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Conference Chairman  J. Boulton
Organizing Committee  M.R. Galley
                             T.R. Lassau
                             A.B. Meikle

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PROGRAM

Monday, June 9th, 1986
Plenary Session with CNA

SESSION A: Fuel and Fuel Channel Materials  
Chairman: A.D. Lane, AECL CRNL

SESSION B: Reactor Physics and Radiation  
Chairman: A. Harms, McMaster University

SESSION C: Safety and the Environment  
Chairman: E.C. Card, W.L. Wardrop & Associates Ltd.

Tuesday, June 10th, 1986
CNS Annual Business Meeting

SESSION D: Fusion I  
Chairman: A.B. Meikle, CFFTP

SESSION E: Thermohydraulics I  
Chairman: D.A. Meneley, University of New Brunswick

SESSION F: Economic and Social Issues  
Chairman: T.R. Lassau, Ont. Research Foundation

SESSION G: Fusion II  
Chairman: R.A. Bolton, Hydro Quebec IREQ

SESSION H: Thermohydraulics II  
Chairman: D.B. Primeau, AECL CANDU Ops.

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Chairman: P. Stevens-Guille, Ontario Hydro
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Slightly enriched uranium fuel cycle: Performance aspects.
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ABSTRACT

Adoption of a slightly enriched uranium (SEU) fuel cycle by CANDU reactors will involve an increase in average fuel discharge burnup to a maximum of 700 MW.h/kg U from the current 200 MW.h/kg U. This paper discusses the significant effects upon fuel performance in increases in burnup, the existing base of pertinent data, and the additional work that is required to verify the technology at levels greater than 400 MW.h/kg U and to raise average discharge burnups to a maximum of 700 MW.h/kg U, or about a factor of three or four greater than the current average of about 200 MW.h/kg U for the once-through natural uranium fuel cycle. These burnups would be attained through enrichment of the fuel to approximately 1.0 wt% U-235 and subsequently to approximately 1.5 wt% U-235 in U respectively.

We are examining the SEU cycle because it has the potential to reduce fuel cycle costs and because it may give added flexibility in exploring various methods of capital cost reduction. A preliminary Ontario Hydro study (1) indicated that the economics of the SEU fuel cycle could already be favourable.

This paper focuses on the fuel performance aspects of the SEU fuel cycle. Areas where the current data base is applicable are discussed and required additional work is identified.

PERFORMANCE ASPECTS

The current once-through natural uranium fuel cycle is performing very well in CANDU reactors. The cumulative defect rate is less than 0.12% with over 380,000 bundles irradiated to date (2). However, additional constraints are placed upon fuel performance at the higher burnups required for the SEU cycle. Areas of particular importance in fuel performance at high burnup are:

1. Waterside corrosion, and deuterium uptake,
2. Fission-gas release and internal gas pressure,
3. Power ramp behaviour,
4. Short-lived fission product release,
5. Bundle integrity,
6. Defect behaviour, and

Fuel clad oxidation and deuterium pickup are influenced by the residence time of fuel in a reactor, by the temperature of the clad, the fluence to which the clad is exposed, and the clad chemistry. Fuel operating powers are similar for the SEU cycle and for the current natural uranium cycle, and fuel burnup is substantially higher. Coolant chemistry will not depend upon the fuel cycle used. Therefore, clad temperatures and coolant chemistry will be similar to current conditions, while both the residence time and the fluence will be increased. However, recent results (3,4) indicate that unless coolant chemistry changes substantially during irradiation, clad oxidation will not be life-limiting, nor will the related deuterium uptake by the clad.

During irradiation, several stable isotopes of the gases Krypton and Xenon are steadily produced, at rates that vary with operating power. Some of the gas precipitates within the UO2 matrix as bubbles, while a fraction of the gas is released to the free volume within the fuel element. A small proportion of the produced gas remains in solution. Fission gases that precipitate as small bubbles contribute to a net volume expansion of the fuel. As irradiation progresses, the bubbles tend to collect on grain boundaries, eventually forming linked networks. Through these networks, and through cracks that are produced in fuel during rapid power changes, fission gases can travel to the free volume at the fuel-clad gap. The release of fission gases is therefore dependent upon temperature, since the produced gases must diffuse to a bubble, grain boundary or crack, and upon burnup. The precise power history is also important, since rapid power changes can change the structure of internal cracks within the fuel. Fission gas release and internal element pressure are important because, if internal pressure rises to an excessive level, heat transfer between the fuel and the sheath can be reduced and a positive feedback loop may be established. Therefore, fission gas release mechanisms at high burnup, and particularly during power ramps at high burnup, require further study.

Numerous effects contribute to dimensional changes in the fuel during irradiation. As fabricated, CANDU fuel contains about 4 vol% of porosity. This porosity is removed during irradiation through irradiation-induced /irradiation-enhanced sintering, at a rate dependent upon por size, temperature and operating power (fission-rate). Alone, this would yield a net volume decrease of the fuel. Early in life, this is the dominant volume-change mechanism, but the rate of volume decrease drops steadily as the amount of available porosity decreases. The steady production of fission products, both solid and gas-
ous, contributes a volume increasing component to the net volume change, which increases monotonically with burnup. Thermal expansion of the fuel will clearly depend upon operating power through fuel temperature. During an increase in power (a power ramp), clad hoop-stresses are increased as the fuel expands. The stress is relieved by straining of the clad. However, the ductility of the cladding is known to decrease with fluence, so relief of the stresses will be slower at higher burnups. In the past, fuel has failed during power ramps by stress-corrosion-cracking of the cladding. These failures were eliminated through careful fuel management and through the introduction of CANLUB coatings. It is of obvious importance to ensure that CANLUB retains its protective qualities at higher burnups.

Following a detailed analysis, accident tasks may be conducted in the Blowdown Test Facility at CKNL. The data obtained from the analysis, and from any tests, will assist with the licensing of the fuel cycle in power reactors.

Fuel performance models and codes will be updated regularly to incorporate the results obtained from high burnup experiments, to ensure continuing predictive capability.

EXISTING DATA BASE

Significant work has been focussed on the high burnup performance of CANDU fuel since the early 1970's. This work was initiated with the realization that advanced fuel cycles in CANDU (i.e. SEU, (Th,U)O₂, (Th,Pu)O₂, and (U,Pu)O₂) would require irradiation to higher burnup than the current 200 MW-h/kg U. Data from thoria-based fuels is not directly applicable to the SEU fuel cycle, but can be applied with caution. Results from (U,Pu)O₂ irradiations are more relevant to the SEU fuel cycle (2), since the fuel compositions are not significantly different, particularly at higher burnups.

The existing data base includes both power reactor experience and experimental work. Over 3000 bundles have been irradiated to above-average burnups, with a few to a maximum of 700 MW-h/kg U, in commercial CANDU power reactors (2). We have also irradiated 8 bundles to high burnup in experimental reactors, supplemented by data from the irradiation of 173 single elements. Some of these single elements were highly instrumented, while others were tests of the effects of changes in particular fuel variables.

The available data show that the current fuel design is capable of reliable operation to burnups of 300-400 MW-h/kg U. However, some preliminary data are suggesting that enhanced fission gas release may be occurring at burnups in excess of 500 MW-h/kg U. Experimental irradiations currently under way should provide a greater understanding of gas release at high burnup. Experiments are being designed for that purpose as well.

DISCUSSION

The preceding has indicated that the performance variables of particular importance at high burnup are fission gas release and power ramp behaviour. We have a large base of data indicating that fuel performance is satisfactory to 400 MW-h/kg U. A development program to introduce SEU with burnups of that order would focus upon code development and verification, and upon the verification of safety. Some power boost tests will also be conducted. A demonstration test at least 40 bundles may be required in a power reactor before fuel burnup could be generally increased to this level. However, at burnups greater than 500 MW-h/kg U, fuel performance is not yet proven. The indications that we have seen of enhanced fission gas release at high burnup require confirmation. Furthermore, it may be necessary to incorporate fuel design changes for gas-release control, such as clearance variation, microstructural control, gas plenums, and central annuli (with or without graphite discs between pellets). To that end we are carrying work on several advanced fuel designs. Model development will also be necessary for burnups greater than 400 MW-h/kg U.

The work required to prove the SEU fuel cycle at and above 400 MW-h/kg U (bundle-average) will focus upon advanced fuel designs, but some work on conventional fuel will also be carried. The tests will take fuel to the extremes of the perceived power history to burnups in excess of that required for the fuel cycle. The experiments will examine the phenomena described above, the fuel performance modelling work will proceed in parallel with the experimental program. The final phase of the program will involve a demonstration in a power reactor of more than 400 bundles, of the selected fuel design, with possible extension to a full core transition. The purpose of this phase of the program is to demonstrate that fuel performance is satisfactory, and that fuel performance is predicted by the behaviour codes.

CONCLUSIONS

The current once-through natural CANDU fuel cycle is performing well. However, there is economic promise associated with the SEU fuel cycle. We have a substantial data base applicable to high burnup, but additional work is required to ensure that power ramp behaviour is adequate, and that the fuel management procedures necessary to minimize power-ramp failures are not too restrictive. Additionally, fission-gas release results from fuel at high burnup are needed. Furthermore, accident behaviour will be studied, and performance models regularly updated.

REFERENCES

(3) MILLER, G.C. and CARTER, T.J., "The Irradiation of 19-Element Bundles to Burnups Greater Than 800 MW-h/kg U in SFR Reactor", to be published (1986).
(4) CARTER, T.J., private communication, 1986 June.
FEAST: A TWO-DIMENSIONAL NON-LINEAR FINITE ELEMENT CODE FOR CALCULATING STRESSES

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ABSTRACT

The computer code FEAST calculates stresses, strains, and displacements. The code is two-dimensional. That is, either plane or axisymmetric calculations can be done. The code models elastic, plastic, creep, and thermal strains and stresses. Cracking can also be simulated. The finite element method is used to solve equations describing the following fundamental laws of mechanics: equilibrium; compatibility; constitutive relations; yield criterion; and flow rule. FEAST combines several unique features that permit large time-steps in even severely non-linear situations. The features include a special formulation for permitting many finite elements to simultaneously cross the boundary from elastic to plastic behaviour; accommodation of large drops in yield-strength due to changes in local temperature; and a three-step predictor-corrector method for plastic analyses. These features reduce computing costs. Comparisons against twenty analytical solutions and against experimental measurements show that predictions of FEAST are generally accurate to ± 5%.

INTRODUCTION

FEAST is a general-purpose finite-element code for calculating static stresses, strains, and displacements in two-dimensional, non-linear systems. It has been used for several analyses of stresses in CANDU fuel, including pellets, sheaths, endcaps, endplates, and plenums. Reference 1 described some recent applications of FEAST in design/analyses of CANDU fuel. These included: hourglassing of UO2 pellets; fatigue of Zircaloy sheaths due to power-cycling; elastic-plastic stress-concentrations at sheath/endcap welds; creep collapse of sheaths; cracking of plenums due to thermal stresses; and stresses in endplates due to gravity.

The present paper focusses on those theoretical aspects of the finite element method that are unique to FEAST. The paper first summarizes the features and capabilities of FEAST, then describes their theoretical details. The paper also compares FEAST predictions to some analytical solutions and to experimental measurements. Two illustrative examples are given.

There are several other stress-analysis codes (Refs. 2, 3), developed independently, that share common features with FEAST. The purpose of the present paper is not to make a detailed comparison of the features of these codes, but to report a portion of the Canadian modelling effort.

The incentive to develop FEAST came from intended applications to nuclear fuel. Therefore, the numerical scheme in FEAST was chosen to minimize computing time in applications involving the following: high temperatures (300-2000°C); extensive brittle cracking (up to half the volume in the mesh); significant plastic flow (local strain approaching 4%); high creep (e.g. 0.3% per hour); and load-histories lasting many years.

FEAST contains no correlations specific to nuclear fuel. Hence it can also be used to analyze other components like pressure tubes, calandria tubes, and even non-nuclear systems.

FEATURES

FEAST accounts for the effects of elastic, plastic, creep, and thermal strains and stresses. Cracking can also be simulated. Since the code is two-dimensional, either plane or axisymmetric calculations can be done. Because the finite element method (Ref. 4) is used, complex geometries, curved surfaces, and unusual boundary-conditions can be accommodated easily and accurately (Refs. 1, 5). Material properties like Young's modulus and plastic modulus can differ in different parts of the structure, depending on local temperature, on prior heat-treatment, and on local strain.

The user can specify arbitrary distributions of stresses, pressures, and temperatures. As well, zero, non-zero, and/or limiting values of displacement can be imposed on parts of the body. These can all be specified as functions of time.

FEAST solves the classical equations of equilibrium (Ref. 6). This ensures that internal stresses balance each other and applied loads at each point. FEAST also ensures compatibility (Ref. 6) among neighbouring fibres. This means that the calculated strains do not create artificial holes, nor do they assign two different points of the material to occupy the same physical space. In addition, FEAST ensures that at each point of the structure, the stress/strain law is consistent with local conditions like temperature and plasticity. The von-Mises equation (Ref. 7) is used to determine if local stresses are elastic or plastic. For elastic elements, the von-Mises yield surface is combined with the Levy-Mises flow rule (Ref. 7). This ensures that

* FEAST - Finite Element Analyses for Stresses
** CANDU - CANada Deuterium Uranium - is a registered trademark of Atomic Energy of Canada Limited
the principal axes of increments in plastic strains, coincide with the principal axes of total stresses. In this paper, plastic strains refer to instantaneous permanent strains. Permanent strains that develop with time, are called creep. The formulations for creep (Ref. 8) are similar to those for plasticity and, in addition, allow for dependence on time.

A three-step predictor-corrector method (Ref. 9) contributes to large time-steps, thus to low computing cost, while maintaining good accuracy for linear and for non-linear problems. FEAST also contains a special formulation (Ref. 10) for elastic-plastic elements. This removes the usual requirement that the time-step go through the knee of the elastic-plastic curve.

For axisymmetric analysis, FEAST prints the following results:
- radial and axial displacements of nodes,
- radial, circumferential, axial, shear, and effective stresses of elements,
- yield strengths of elements,
- radial, circumferential, axial, shear, and effective strains of elements,
- principal stresses and their directions,
- minimum and maximum values of the above components of stresses and strains, and their locations.

Similar parameters are also printed for plane-stress calculations. In addition, FEAST saves the strains and stresses on a magnetic tape, for post-processing and for plotting of contours.

FRAMEWORK

The finite element method (Refs. 4, 5, 9) divides the analyzed region into a number of smaller, idealized subregions called finite elements. The elements are connected to each other at the corner-points, called nodes. Figure 1 shows some triangular elements and nodes. Equations relating forces to displacements are formed for each finite element. These equations account for the shape, size, and location of each element, for the type of load (e.g. axisymmetric expansion), and for the physical processes (e.g. plasticity, creep). Then these equations are assembled to describe the entire system. The system equations are modified to account for boundary conditions. The equations are then solved to obtain displacements, strains, and stresses.

The following paragraphs describe the theory behind FEAST. 'Plane stress' calculations represent a special case of 'axisymmetric' calculations. Hence, the more general 'axisymmetric' option is used here to illustrate the principles. 'Plane stress' calculations use similar principles, and are not discussed in this paper for brevity. Tensor notation usually simplifies the description of the theory of the finite element method. However, the matrix notation (Ref. 4), though cumbersome, is more widely understood, hence is used in this paper. The symbols are defined towards the end of the paper in the section 'Nomenclature'. For axisymmetric structures, the matrices contain four rows in the following order: radial, circumferential, axial, and shear. Shear refers to the r-z plane.

In the displacement approach given by Zienkiewicz (Ref. 4), force balance provides the following equation for a finite element:

\[ \{dF\}^T = [K]^T \{d\sigma\}^T + \{dP\}^T_{ext} + \{dF\}^T_{int} \]  

By using the principle of virtual work, the following equations can be obtained (Ref. 4):

\[ \{d\epsilon\} = [B] \{d\sigma\} \]  

\[ \{d\theta\} = [B] \{d\epsilon\} \]  

\[ [K]^T = \int \int \{B\}^T [B] \{\epsilon\} \, dV \]  

\[ \{d\epsilon\} = \int \int \{B\}^T \{\epsilon\} \{d\epsilon\} \, dV \]  

Zienkiewicz (Ref. 4) gives the explicit equations for \([B]\). The equations for the slope matrix \([B]\), and for the force vector \([dF]\), depend on the physical process considered, e.g. elasticity, plasticity, creep, cracking. Their derivations are discussed later in this paper.

By repeated application of equation 4, the stiffnesses \([K]^T\) of all finite elements are obtained. They are then assembled into a global stiffness matrix, which describes the stiffness of the entire system.
This process gives a set of simultaneous linear equations relating known external loads \( \{dR\} \), to unknown nodal displacements \( \{d6\} \), via the known global stiffness \( \{K\} \), as follows:
\[
\{dR\} = \{K\} \{d6\} + \{dF\}_0 + \{dF\}_I
\]  
(7)

Solution of equation 7 provides displacements, strains, and stresses.

The above equations are equally valid for linear and for nonlinear problems. Plasticity and creep make the problem nonlinear. Hence FEAST solves equation 7 incrementally. The total load is divided into a series of smaller loads. The total values of displacements, strains, and stresses are the sum of previous total plus the current increment.

Each increment of load is kept reasonably large by using a three-step predictor-corrector method (Ref. 9). It uses three iterations per load-step (also called time-step). Figure 2 shows this schematically. Point 0 represents the solution at the end of the previous time-step. To discuss Figure 2, let us assume that the temperature is higher during the current time-step, giving additional thermal strain and lower yield strength. Points 1, 2 and 3 represent the solutions during the current time-step, after iterations 1, 2, and 3 respectively.

The first iteration accounts for the drop in yield strength due to increase in local temperature. It also corrects for residual errors from the previous solution.

The second iteration applies half the load-increment. This provides the average stiffness of the system during the time-step.

The final iteration applies the full increment in load. It uses the average stiffness calculated in second iteration, and thus provides a reasonable calculation of final displacements and stresses for the time-step.

To solve equation 7, we now need to derive the slope matrices, \( \{D\} \), and the force vectors, \( \{dF\} \), for the modelled processes: elasticity, thermal strain, cracking, plasticity and creep. These are discussed in the following five sections. Table 1 compiles the resulting equations for the slope matrices \( \{D\} \).

**ELASTIC AND THERMAL STRAINS**

If the stresses are in the elastic range, FEAST uses the equations given by Zienkiewicz (Ref. 4) for slope \( \{D\} \), see Table 1. Similarly, thermal expansion is treated as an initial strain. This is also calculated by the equations given by Zienkiewicz (Ref. 4):
\[
\{d\sigma\}_I = \alpha (d\dot{u}) \{1, 1, 1, 0\}^T
\]  
(8)

Equation 8 is used in equation 5.

**CRACKING**

FEAST permits 'radial' cracks to develop in the system, see Figure 3, if the hoop stress exceeds the fracture stress. 'Plane stress' calculations are done for cracked finite elements. The \( \{D\} \) matrix for cracked elements is modified to give zero hoop stress, see Table 1. In addition, one must account for the redistribution of the hoop stress that was present in the element prior to the formation of the crack. The correction takes the form of an initial stress. The resulting incremental forces are given by (Ref. 11):
\[
\{dF\}_I = \int \{B\}^T \{0, -\sigma_0, 0, 0\}^T dV
\]  
(9)

These nodal forces are used in equation 6.

**PLASTICITY**

For plasticity calculations, FEAST considers permanent strains that are instantaneous, independent of strain-rate, and independent of thermal activation.

FEAST accounts for the following six features (Ref. 7) of incompressible plastic flow in ductile materials:

1. The components of stresses combine in such a manner that the effective stress lies on the yield surface;
2. The size of the yield surface depends on local temperature;
3. Due to work-hardening and/or strain-softening, the slope of the stress-strain curve is a function of accumulated strain;

**EXAMPLES OF POSSIBLE CRACKS IN A NUCLEAR FUEL ELEMENT**
TABLE 1
SUMMARY OF THE DIFFERENT [D] MATRICES CORRESPONDING TO THE VARIOUS MATERIAL BEHAVIOUR THAT AN ELEMENT MAY EXHIBIT

<table>
<thead>
<tr>
<th>Elastic behaviour</th>
<th>Plane stress, or radially cracked</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>[D]</strong>^e</td>
<td><strong>[D]</strong>^p,ck</td>
</tr>
<tr>
<td>Elastic and plastic behaviour</td>
<td>Elastic and plastic behaviour</td>
</tr>
</tbody>
</table>

4. The principal axes for increments in plastic strain coincide with the principal axes for total stresses;
5. Plastic flow does not change the volume of the material; and
6. Work done is positive during plastic flow, i.e., plastic strains are not recovered by removing loads.

The above features require changes in the slope matrix [D] of equation 3, and in the initial load vector \([dF]_{0}\) of equation 6. This section describes the pertinent equations used in FEAST for [D] and \([dF]_{0}\) of plastic material.

The incremental theory of plasticity (Ref. 7) shows good agreement with experiments. In the incremental theory, the above physical principles are expressed by the following three equations:

Total plastic strain:

\[
[\text{dc}] = [\text{dc}]^{\text{elas}} + [\text{dc}]^{P} + [\text{dc}]^{t}
\]  (10)

Levy–Mises flow rule (Ref. 7):

\[
d\lambda = \frac{(\text{dc})^{P}}{\sigma} = \frac{(\text{dc})^{P}}{\sigma_{r}} = \frac{(\text{dc})^{P}}{\sigma_{z}} = \frac{(\text{dc})^{P}}{2t}
\]  (11)

von-Mises yield function (Ref. 7):

\[
2\sigma^{2} = (\sigma_{r} - \sigma_{0})^{2} + (\sigma_{0} - \sigma_{z})^{2} + (\sigma_{z} - \sigma_{r})^{2}
\]  (12)

It is convenient to express the yield function (equation 12) in its incremental form:

\[
\frac{2}{3} \sigma (\text{dc}) = \sigma_{r} (\text{dc}_{r}) + \sigma_{z} (\text{dc}_{z}) + 2t (\text{dt})
\]  (13)
Equations 10, 11, and 13 provide three linear equations in three unknowns: \([d\epsilon]^P, d\lambda,\) and \(d\varphi.\) By following the procedure given by Yamada (Ref. 12), the three unknowns are eliminated by simultaneously solving the three equations. Then, equation 3 is used to express the elastic strains in terms of stresses. This yields the following expression:

\[
[d\sigma] = [D]^P \cdot [d\epsilon] + [d\sigma_0]
\]  

(16)

Equation 14 provides an explicit relation between stresses and strains. Compared with some other formulations (Ref. 13) that require inversion of matrices, equation 14 results in low computing time.

The slope matrix, \([D]^P,\) is given in Table 1. The last term in equation 14 represents thermal stresses, and is similar to equation 8.

The vector \([d\sigma_0]\) simulates the reduction in flow stress due to an increase in local temperature. This feature was especially formulated for FEAST, and aids in large time-steps. For axisymmetric solids, \([d\sigma_0]\) is given by:

\[
[d\sigma_0] = \frac{d\sigma^T}{m^2} \cdot [\sigma']
\]

(15)

where \(m = 1 + \frac{2}{3} \frac{(1 + \nu)^P}{E}\)

(16)

For radially cracked elements, \([d\sigma_0]\) is given by:

\[
[d\sigma_0] = \frac{2}{3} \frac{d\sigma^T}{m^2} \cdot [\sigma']
\]

(17)

where:

\[
[S'] = \frac{[S]}{S_r}, \frac{[S]}{S_r}, \frac{[S]}{S_r}
\]

\[
S = \frac{4}{9} \frac{\sigma^2}{\nu^2} \cdot P + \sigma_0 \cdot S_r + 2\sigma_0 \cdot S_3 + 2\sigma_0 \cdot S_4
\]

\[
S_r = \frac{E}{1 - \nu^2} \cdot (\sigma_0^2 + \nu \sigma_0^2)
\]

\[
S_3 = \frac{E}{1 - \nu^2} \cdot (\nu \sigma_0^2 + \sigma_0^2)
\]

\[
S_4 = \frac{E}{1 + \nu} \cdot \tau
\]

For plastic materials, \([D]^P\) is used instead of \([D]\) in equation 3. The \([d\sigma_0]\) term of equation 6 is obtained from equations 15 and 17.

ELASTIC AND PLASTIC BEHAVIOUR

Many non-linear codes for stress analysis require that all finite elements that start out elastic at the beginning of a time-step, must also stay elastic during that entire time-step. This restraint simplifies the calculation of slope matrices \([D]\) for individual finite elements.

But it also means that each finite element must go through the knee of the stress-strain curve, see Figure 2. For applications typical of nuclear fuel, this frequently results in a large number of very small time-steps, thus large computing cost.

A special formulation in FEAST avoids this cost. Large time-steps are used. In each increment, many elements are allowed to exhibit different combinations of elastic and plastic behaviour. Weighted stress-strain relations are used to calculate the slope matrices \([D]\) of finite elements that go from elastic to plastic state during a time-step. The resulting slope matrix is given in Table 1. It is important to accurately calculate the weighting function \(W.\) FEAST uses the method described briefly in Reference 10 and in more detail in Reference 14. The method consists of first defining the load-path in the six-dimensional stress-space. Then, the intersection is found between the load-path and the yield-surface. For the von-Mises yield function, this results in a quadratic equation in the six-dimensional space. Its solution is obtained by the normal methods of algebra. Further details are available from References 10 and 14. This is a major feature of FEAST and permits large calculation-steps.

CREEP

For creep calculations, FEAST considers permanent strains that develop with time. The model incorporates the following physical features (Ref. 8): (i) creep does not change the volume of the material; (ii) the principal axes for increments in creep strains coincide with the principal axes for total stresses; and (iii) positive work is done by external forces. These features are similar to those for plasticity, and their mathematical description (Ref. 8) is provided by equations 10, 11, and 13.

The creep rate of the material, \(\vec{\varepsilon}_c,\) is assumed to be known as a function of local stress, temperature and strain. That is,

\[
\varepsilon = \varepsilon_c (\vec{\sigma}, T, \vec{\varepsilon})
\]

(18)

Above equation is combined with equations 10, 11 and 13. Then, mathematical manipulations are done similar to plasticity, and an equation similar to equation 14 is obtained. The resulting slope matrix \([D]\) for creep is given in Table 1.

BOUNDARY CONDITIONS AND SOLUTION PROCEDURE

We now have all the ingredients for equations relating nodal forces to nodal displacements of the entire system (equation 7). They are assembled using the standard procedure (Ref. 4) of the finite element method. Fresh equations are assembled for each time-step, and for each iteration. The appropriate terms in the stiffness matrix of the system are modified to reflect the boundary conditions, as suggested by Zienkiewicz (Ref. 4). This method requires less computing time than the method of altering the size of the stiffness matrix.
Equation 7 is solved by using the method of Gaussian elimination and back-substitution. This provides nodal displacements. Equation 2 then gives strains, and equation 3 gives stresses.

**ACCURACY**

The predictions of FEAST have been compared against closed-form solutions for about 20 cases. They include combinations of plane-stress, axisymmetry, elasticity, thermal stresses, plasticity, drop in yield strength, and creep. The agreement between FEAST and closed-form solutions is usually within ±5%.

Reference 1 reported the excellent agreement of FEAST predictions with closed-form solution for creep stresses in a long, thick, closed, internally pressurized cylinder. This paper reports three more comparisons: (i) concentrations of elastic stresses near a circular hole in a rectangular plate subjected to tension; (ii) elastic-plastic stresses in the same plate; and (iii) elastic-plastic stresses in a long, thick, internally pressurized cylinder.

**Case 1: Elastic Stresses in a Plate**

Figure 4(a) shows the rectangular plate simulated on FEAST. The plate has a small hole at its center. Uniform uniaxial tension \( \sigma \) is applied in the x direction. This results in non-uniform stresses near the hole (Ref. 15). The stress-concentration is defined as the local value of normal stress in the x direction \( (\sigma_x) \), divided by the applied tension \( (\sigma) \). Figure 4(b) shows the predictions of FEAST for stress concentrations at different points along the y-axis. There is adequate agreement between predictions of FEAST and of closed-form solution (Ref. 15). The agreement can be improved further, if needed, by refining the mesh.

**Case 2: Elastic-Plastic Stresses in a Plate**

In the problem of Figure 4(a), the peak value of effective (von-Mises) stress occurs at point \( x=0, y=a \). Hence, plastic flow occurs first at that point. We increased the tension applied on the plate, until the peak effective stress was well in the plastic range. Figure 5 shows predictions of FEAST for the peak effective stress, as a function of applied external load, and compares them to the closed-form solution of Tuba (Ref. 16). Plastic flow redistributes stresses, and the agreement between Tuba's solution and FEAST improves as the load increases.

**Case 3: Elastic-Plastic Stresses in a Cylinder**

Case 3 studied the central region of a long, thick, internally pressurized, axially loaded, cylinder. Axial strain was not allowed, resulting in conditions of plane strain. Figure 6 shows the test case, and the results. Finite element predictions are in reasonable agreement with closed-form solutions (Ref. 17).

**VALIDATION**

A comparison is available against strain-gauge measurements for compression tests on endcaps of fuel elements. Figure 7 shows that the predictions of the finite element code agree with measured gradients for hoop strains, to within ±5%.
ILLUSTRATIVE EXAMPLES

FEAST has been used for the following applications in CANDU fuel:

- Estimate the hourglassing of UO₂ pellets. These calculations are related to stress-corrosion-cracking of sheaths.

- Determine stresses in Zircaloy sheaths at circumferential ridges. This is related to assessing the integrity of sheath during fatigue due to power-cycling in a CANDU-600 reactor.

- Calculate stresses at welds between sheaths and endcaps. This study was related to fuel failures (Ref. 18) in Unit 3 of the Bruce reactor, in 1984. The suspected cause of the failures was delayed-hydrogen-cracking (Ref. 18).

- Assess the influence of the location of discontinuity in sheath/endcap weld, on the load-carrying capacity of the bond.

- Check the stability of sheath during creep collapse due to coolant pressure, on pellets of small diameter.

- Assess thermal stresses in graphite plenums, to explain the observed cracking at corners.

FIGURE 6 ELASTIC-PLASTIC STRESSES IN A CYLINDER: FINITE ELEMENT VS. CLOSED-FORM SOLUTION
Example 1: Endcap Optimization

This axisymmetric analysis investigated the possibility of structural optimization of the internal design of the endcap. It considered the resistance of the endcap against ductile failure due to concentric axial loads during refuelling. Figure 8 shows the effective (von-Mises) stresses in two hypothetical endcaps of Pickering-size fuel. For reasons of commercial proprietary, the geometries shown in the figure do not represent real endcaps; they are used here to illustrate the capabilities of FEAST. Compared to geometry #1, geometry #2 gives a more uniform distribution of effective stress. Geometry #2 also uses less Zircaloy and provides more volume for storing fission gas.

Example 2: Endplate Stresses

This study assessed the stresses in the endplate of a Pickering fuel bundle. The endplate was assumed to carry the gravity loads of the bundle (elements plus endplates). Plane-stress conditions were assumed, ignoring those bending moments which are not in the plane of the endplate. Figure 9 shows the endplate, and the locations of the fuel elements. As expected, the bottom half of the endplate has the largest stresses. Stress concentrations are highest at the corners at the two ends of the bottom spoke. This is consistent with the expected perturbations in stress-flowlines, at re-entrant corners (Ref. 6). The maximum effective stress is 27 MPa.
6. FEAST has been used for several analyses of stresses in CANDU fuel, including pellets, sheaths, endcaps, sheath/endcap welds, plenums, and endcaps. It can also be used for applications other than CANDU fuel, e.g., pressure tubes and calandria tubes.

ACKNOWLEDGEMENTS

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NOMENCLATURE

Subscripts
- \( o \) : initial value
- \( r, \theta, z \) : polar coordinates
- \( x, y, z \) : cartesian coordinates
- \( c \) : creep
- \( ck \) : crack
- \( e \) : element
- \( elas \) : elastic
- \( P \) : plastic
- \( t \) : thermal
- \( T \) : transpose
- \( \sigma \) : deviatoric
- \( \tau \) : effective

Superscripts
- \( c \) : creep
- \( ck \) : crack

Symbols

\[ \{ \] represents a column vector, in the following order: radial, circumferential, axial, shear

\[ [ \] \] represents a rectangular matrix

\[ [B] \] : matrix relating strains to displacements

\[ d \] : infinitesimal increment

\[ [D] \] : slope relating strain-matrix to stress-matrix

\[ E \] : Young's modulus
\( \mathbf{F} \) : Force vector

\( \mathbf{K} \) : Stiffness matrix

\( m \) : Factor defined by equation 16

\( \mathbf{R} \) : Vector of external forces at nodes

\( V \) : Volume of the finite element

\( W \) : Weighting Factor

\( \alpha \) : Coefficient of linear thermal expansion

\( \mathbf{\theta} \) : Vector of displacements at nodes

\( \mathbf{\varepsilon} \) : Strain vector

\( \dot{\varepsilon} \) : Creep strain rate

\( d\lambda \) : Constant of proportionality in the flow rule

\( \nu \) : Poisson's ratio

\( \mathbf{\sigma} \) : Stress vector

\( \Delta \sigma^t \) : Change in yield strength due to increase in local temperature

\( \tau \) : Shear stress

\( \omega \) : Temperature

REFERENCES


ABSTRACT

At present, operating reactors provide the most reliable data on elongation rates for Zirconium-Niobium pressure tubes in CANDU reactors. The reactor data indicate that there are differences in elongation rates across the reactor face and between reactors, especially between those reactors having substantially different operating conditions, that cannot be explained by differences in the operating flux levels. Although it was initially believed that the flux in some way contributed towards this variability in elongation rate in that the rate was not directly proportional to flux, the general conclusion of this paper is that temperature and ultimate tensile strength make the greatest contribution to the variability.

INTRODUCTION

Neutron irradiation, combined with reactor operating conditions of high temperature and high internal pressure, were expected to cause in-service dimensional changes in CANDU pressure tubes, primarily by irradiation creep and/or growth. However, in the design of early CANDU reactors, the predominant dimensional change anticipated was diametral creep, not axial elongation, inasmuch as the hoop stress from internal pressure was known to be higher than the axial stress. Consequently, allowance for significant axial elongation was not considered to be necessary, and CANDU reactors up to and including Bruce Unit 3 were designed to accommodate only limited pressure tube axial elongation.

Notwithstanding, in-service elongation measurements have shown axial elongation of pressure tubes to be significant. Thus early CANDU reactors will encounter problems due to insufficient length of the fuel channel support bearings and limited allowance in the yoke assemblies used to position the channels axially. Later designs have provided adequate bearing travel and adjustable yoke assemblies to lock the fuel channel axially at one end only. Remedial action, designated REPAB (Repositioned End Pinnings And Bearings), has been planned for these reactors (Pickering Units 3 and 4, and Bruce Units 1 to 3). There are also problems associated with the tube-to-tube variations in elongation rate, which can lead to interference between feeder pipes and with the operation of the fuelling machines.

The accommodation of significantly more pressure tube elongation in later designs has complicated the design of the fuel channels and thus made them more costly. Therefore it is desirable that in-service dimensional changes of future pressure tubes be smaller and more uniform, resulting in less complex and costly restraint and support devices.

Considerable effort has been expended to identify the parameters that govern the creep and growth of pressure tubes. This paper presents an analysis of the effect of various parameters, e.g., reactor operating conditions and pressure tube material properties. The thesis is that the operating reactors themselves provide the most reliable data base for such an evaluation. Pressure tube samples irradiated in high flux test reactors do provide complementary data, however, in such irradiations neither the actual operating conditions nor the geometrical effects of the pressure tubes are simulated. Therefore such data may be used only to indicate long term trends.

Examination of the reactor data indicates that there are differences in pressure tube elongation rates across the reactor face and between reactors, especially between those reactors having substantially different operating conditions, that cannot be explained by differences in the operating flux levels. Hence the reactor measurements themselves can be applied only to short term predictions of such things as channel on-bearing life and then only for that particular reactor. Before data from one reactor can be used for predicting elongation rates in another reactor an understanding of the fundamental basis for the difference in rates is needed. This understanding can only come from a thorough analysis of the pressure tube data with respect to all significant parameters. This paper describes such an analysis.

The analysis described here is an evaluation of the performance of pressure tubes in Pickering NGS A, Units 3 and 4 and Bruce NGS A, Unit 2. Fortunately, for these three reactors there is an abundance of pertinent data. The elongation of the pressure tubes has been measured periodically since start-up, there is a mixture of tube types (Table 1) and a detailed operating history is available for each channel. Although it was initially believed that neutron flux contributed in some way towards this variability in elongation rates in that the rate was not directly proportional to flux, the general conclusion of this paper is that temperature and ultimate tensile strength make the greatest contribution to the variability. There appears now to be a good basis for prediction of pressure tube elongation rates in reactors operating under conditions similar to
Pickering and Bruce, but additional data points, preferably from other reactor units, are required to provide the necessary validation.

### TABLE 1: TYPE AND QUANTITY OF PRESSURE TUBES

<table>
<thead>
<tr>
<th>Reactor Unit</th>
<th>Tube Type</th>
<th>Qty</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pickering 3</td>
<td>Regular Pickering A</td>
<td>172</td>
</tr>
<tr>
<td></td>
<td>Replacement Pickering A</td>
<td>3</td>
</tr>
<tr>
<td></td>
<td>Replacement Bruce A</td>
<td>14</td>
</tr>
<tr>
<td></td>
<td>Replacement Darlington A</td>
<td>1</td>
</tr>
<tr>
<td>Pickering 4</td>
<td>Regular Pickering A</td>
<td>328</td>
</tr>
<tr>
<td></td>
<td>Replacement Pickering A</td>
<td>2</td>
</tr>
<tr>
<td></td>
<td>Replacement Bruce A</td>
<td>50</td>
</tr>
<tr>
<td></td>
<td>Eldorado (as billets)</td>
<td>6</td>
</tr>
<tr>
<td></td>
<td>Bruce (as billets)</td>
<td>3</td>
</tr>
<tr>
<td></td>
<td>Replacement Darlington A</td>
<td>1</td>
</tr>
<tr>
<td>Bruce 2</td>
<td>Regular Bruce A</td>
<td>270</td>
</tr>
<tr>
<td></td>
<td>Bruce A (Beta-quenched)</td>
<td>152</td>
</tr>
<tr>
<td></td>
<td>Eldorado (as billets)</td>
<td>49</td>
</tr>
<tr>
<td></td>
<td>Pickering A</td>
<td>9</td>
</tr>
</tbody>
</table>

### PRESSURE TUBE ELONGATION RATES

#### Elongation Versus Fluence

The starting point for this analysis is the individual elongation measurements of pressure tubes for Pickering Units 3 and 4 and Bruce Unit 2 taken with the STEM measuring equipment. The elongation measurements for Pickering Unit 3 are shown plotted against the calculated channel neutron fluence in Figure 1. Several observations can be made from this plot:

1. The large spread in the data points at any particular value of elapsed time in reactor Effective Full Power Hours (EFPH) indicates that, on a time basis, the pressure tubes are not elongating at the same rate;

2. The width of the scatter band indicates that the elongation of the tubes is not necessarily equal at any specific value of neutron fluence;

3. The spread of the data points along the diagonal at any particular value of EFPH indicates that the tubes are not necessarily accumulating fluence at the same rate;

4. A linear regression would not accurately represent the data points inasmuch as the average values of the data points appear to depart from a true straight line at higher fluences.

As the neutron flux is known to vary across the face of the reactor, as shown in Figure 2 for the Pickering and Bruce reactors, it would not be expected that the tubes would necessarily accumulate fluence at the same rate, thus supporting observation 1, above. However, this does not explain observation 2, that tubes do not necessarily have the same elongation at any specific value of neutron fluence.

To obviate the effect of the variation in flux across the reactor face on the time for accumulation of fluence, the analysis in this paper has been based on expressing the pressure tube elongation rate in terms of unit elongation per unit neutron fluence, i.e., millimeters per neutron per square centimeter.

Explanations will be given later for observations 2 and 4, above.

Elongation measurements for typical individual tubes are shown plotted against neutron fluence in Figure 3. The elongation rate for each tube, expressed as elongation per unit fluence, has been calculated by performing a linear regression on the data points. The closeness of the fit is expressed by the value of "r". A value of 1.0 is a perfect fit. These results suggest that, though elongation rates vary from tube to tube, tube elongation is not random scatter as could be assumed from a first glance at Figure 1. This may address the parameters that cause this variation in elongation rate between tubes.
Elongation Rates Versus Flux and Temperature

In Figures 4 and 5 average elongation rates for pressure tubes in each reactor have been plotted against average neutron flux. The tubes having the highest neutron flux would be those located near the center of the reactor and those with the lowest flux, at the periphery. A number of observations can be drawn from these plots:

5) Neutron flux appears to contribute towards the variability in elongation rates in that the rates vary with neutron flux for both Pickering Unit 3 and the outer zone of Bruce Unit 2 (504°C inlet temperature), i.e., those tubes at the center of the reactor having the highest elongation rate and those tubes at the periphery the lowest;

6) Elongation rates for the central zone of Bruce Unit 2 (484°C inlet temperature), appear to be vary inversely with neutron flux;

Note: The core in Bruce is divided into two temperature zones, an outer hot zone and an inner cooler zone, due to the preheaters which are located in the coolant circuit for the inner zone.

In Figures 4 and 5 plots of average channel temperature versus average neutron flux have also been provided for both reactors. From these temperature plots it is obvious that average channel temperature correlates with average neutron flux, therefore it can be equally stated that elongation rates appear to be a function of temperature. This apparent relationship with temperature is shown in Figures 6 and 7.
In Figure 8 average pressure tube elongation rates have been plotted against average neutron flux for three temperature profiles and superimposed on the plot of Figure 4 for Pickering Unit 3. In Figure 9 average pressure tube elongation rates have been plotted against average channel temperature for two neutron flux profiles and superimposed on the plot of Figure 6 for Pickering Unit 3. From these plots it is concluded that flux has very little influence on elongation rates, the predominant parameter being temperature.

To establish the relationship between tube elongation rate and temperature the plot for Pickering Unit 3 in Figure 6 and the plot for the outer zone of Bruce Unit 2 in Figure 7 have been placed together in Figure 10 and a linear regression line drawn through all the data points. Normalizing the plots for elongation rate versus neutron flux for an average temperature of 260°C yields the flux plots of Figure 11 for Pickering Unit 3 and Bruce Unit 2. Evidence to the applicability of this approach is given in Table 2 where slopes of the linear regression lines through the data points for Pickering Units 3 and 4 and Bruce Unit 2 are compared with that of the regression line for the combined plot. An anomaly exists for Pickering Unit 4 in that the channel average temperatures must be reduced by 7°C in order for the data points to fall on the regression line for the combined plot. A further anomaly exists for the inner zone of Bruce Unit 2 where the plot for the data points has a negative slope. These two anomalies may be related to errors in the calculated channel temperatures or to other parameters which affect pressure tube elongation rate but have not yet been specifically identified.
TABLE 2: COMPARISON OF REGRESSION LINE SLOPES
ELONGATION RATES VS TEMPERATURE

<table>
<thead>
<tr>
<th></th>
<th>Slope</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pickering Unit 3</td>
<td>0.142</td>
</tr>
<tr>
<td>Pickering Unit 4</td>
<td>0.105</td>
</tr>
<tr>
<td>Bruce Unit 2 (outer)</td>
<td>0.117</td>
</tr>
<tr>
<td>Combination (P-3 &amp; B-2)</td>
<td>0.111</td>
</tr>
</tbody>
</table>

Elongation Rates Versus UTS and %CW

In Figures 12 and 13, pressure tube elongation rates for Pickering Unit 3 have been plotted against Ultimate Tensile Strength (UTS) and % Cold Work (%CW), respectively. Linear regression lines are shown for each plot. It appears that there is a relationship between pressure tube elongation rate and both UTS and %CW, the former being the stronger.

In Figure 14, a normalized plot of average elongation rate versus flux for an average UTS of 70,000 psi (482.6 MPa) and a temperature of 260°C has been superimposed on a flux plot normalized only for temperature. This figure shows that normalizing for UTS shifts the curve upwards. It also shows that UTS is randomly distributed, as would be expected.
Empirical Equation For Pressure Tube Elongation

The following empirical equation expresses the difference in elongation rates between tubes. It is based upon the linear regression lines for effect of temperature and UTS derived above.

\[
\text{Elongation rate} = 0.210 T + 41.136 \text{UTS} - 0.00141 T \times S - 42.235
\]

Where

- \( T \) = temperature in °C
- \( \text{UTS} \) = ultimate tensile strength in MPa
- \( S \) = stress (\( \text{mm/} \text{N/cm}^2 \) * 10**2.21)

For a temperature difference of +25°C, this equation gives an increase in pressure tube elongation of approximately 40% compared to 10% for the CRNL empirical equation.

Elongation Rates Versus Tube Manufacture

Tubes Manufactured For Other Reactors. In Figure 15, the elongation rates for replacement tubes, some manufactured for Pickering and others for the Bruce A reactor, are compared with the elongation rates for tubes manufactured specifically for the Pickering reactor. All data has been normalized to a temperature of 260°C. There is no significant difference in the elongation rates, those for the Bruce replacement tubes being only slightly lower than those for the regular tubes in Pickering Unit 3, and the Pickering replacement tubes exhibiting similar characteristics to the regular tubes.

Tubes Manufactured From Logs From a Different Source. In Figure 18, the average elongation rates of tubes manufactured from billets fabricated by Eugine (France) and Eldorado are compared with those of the regular Pickering tubes. It is evident that the tubes produced from billets manufactured by another organization have substantially different elongation rates than the regular Pickering tubes. The rates for the Eugine tubes are higher while those for the Eldorado tubes are lower. This would suggest that the manufacturing process leading up to the billet stage may have a significant impact on the elongation properties of the final product.

Tubes Manufactured From Same Logs. In Figures 16 and 17, two comparisons of the elongation rates for regular Pickering tubes are made: on each figure, between Pickering Units 3 and 4, and between the figures, for and regular tubes manufactured from the same logs (Figure 16) and all other regular tubes (Figure 17). The apparent difference in elongation rates between Units 3 and 4 in Figure 16 also exists in the tubes manufactured from the same logs, proving that the difference is not related to tube manufacture but to the reactor operating conditions. It was noted above that an error in temperature measurement of +7°C would account for this anomaly (Unit 4 is reportedly operating hotter, not cooler, than Unit 3).
Tubes Manufactured With a Different Heat Treatment.

In Figure 19 the average elongation rates of Bruce tubes given a Beta quench heat treatment in the log stage are compared with those for the regular Bruce tubes, for the outer zone of Bruce Unit 2. The Beta quenched tubes appear to have a slightly lower elongation rate, again suggesting a possible manufacturing parameter that should be assessed more fully for its impact on pressure tube elongation rates.

CONCLUSIONS

The following conclusions have been drawn from the described analysis of pressure tube elongation rates in Pickering Units 3 and 4 and Bruce Unit 2:

(1) Each tube exhibits a constant elongation rate governed by its particular environment and manufacturing history;

(2) Variableness of pressure tube elongation rates appears to be temperature, not flux, dependent;

(3) Pressure tube elongation rates appear to be dependent on UTS, a cold work as well as on heat treatment and the tube manufacturing process up to the billet stage;

(4) There appears to be no significant difference in the elongation rates of Bruce and Pickering tubes.

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The contributions of E. Lam, G. Hiquchi, V. Yu and L.S. Sodhi are gratefully acknowledged.
ABSTRACT

The issue of integrity of pressure tubes in CANDU reactors in accident conditions is examined based on the potential for local failure. The significance of the main parameters that influence the local strain failure are studied by a detailed parametric analysis. Based on this study a set of failure maps are presented for assessment of pressure tube integrity.

1.0 INTRODUCTION

In a CANDU reactor the pressure tubes form the main coolant pressure boundary within the reactor core. Consequently they are designed to withstand all anticipated operating conditions both normal and abnormal, based on the ASME requirements for Class I pressure vessels. However, during certain postulated accident scenarios, such as a loss of coolant accident (LOCA), the pressure tubes could be subjected to severe temperature transients while the coolant system pressure is still relatively high. One of the aims of safety analysis is to demonstrate the integrity of the pressure boundary during these transient pressure and temperature loads. Traditionally in the safety analysis, the integrity of the pressure tube during these transients is demonstrated in a two step approach. First a detailed thermal hydraulic analysis is carried out to predict the pressure and temperature transients and in the second step using this data the integrity of the pressure tubes is assessed by a detailed analysis of deformation behaviour during the transient. For a rapid and accurate assessment of pressure tube integrity, it is very convenient if the conditions leading to pressure tube failure are known beforehand.

Theoretical and experimental studies have been reported in Ref (1-4) which have attempted to indicate the conditions required for failure of pressure tubes during high temperature transients. Based on these data a set of failure maps for pressure tubes have been reported in Ref (5) which is derived on the basis of a cosine temperature distribution around the circumference. This paper presents the conditions required for failure of the pressure tube in the form of failure maps in a more generalized form.

2.0 DEFINITION OF THE PROBLEM

The pressure tubes in CANDU reactors are separated from the surrounding calandria tubes by a gas annulus to prevent excessive heat loss to the cool moderator during normal operating conditions. However, during certain postulated accident scenarios, the moderator is credited as the ultimate heat sink for removal of decay heat from the fuel. To credit the moderator as the ultimate heat sink it is necessary to show that the pressure tube balloons uniformly to come into contact with the calandria tubes thus maintaining the integrity of the pressure boundary and establishing a good heat transfer path to the ultimate heat sink. The amount of pressure tube diametral strain required to balloon and come into contact with the calandria tube is approximately 18 percent. When the heat transport system pressure is relatively high and if the circumferential temperature distribution on the pressure tube is relatively uniform, the pressure tube can indeed achieve this order of strain by creep deformation, as observed in contact boiling tests (Reference 6).

There are a number of accidents considered for licensing and safety analysis purposes which invariably lead to non-uniform temperatures on the pressure tubes. Some of these accident situations are:

(a) Stratified flow conditions in a fuel channel.
(b) Non-uniform heatup during LOCA.
(c) Local hot spots due to fuel element/pressure tube contact or due to molten Zircaloy dropping on the pressure tube.

Under stratified coolant flow condition the portion of the pressure tube exposed to the uncovered fuel can heat up at a higher rate due to radiative heating. This situation can also arise if the fuel is cooled by natural circulation. In such cases the pressure tube experiences non-uniform creep deformation which results in not only an increase in the diameter of the pressure tube but also a non-uniform reduction in thickness. Generally, the reduction in thickness is greatest at the point of highest temperature in the pressure tube.

Due to the non-uniform creep deformation the pressure tube may either contact the calandria tube or it can fail locally at the hottest point by wall thinning before contact. To assess the potential for local failure a failure criterion in the form of an upper bound local strain is needed. If the average circumferential strain in the PT is 18 percent or more before the local strain at the hot spot reaches the failure limit, failure of the PT is precluded due to its contact with the cold calandria tube (CT) and channel integrity is assured. As a result the issue of PT integrity reduces to the task of establishing the temperatures at which rapid strain accumulation is possible at the hot spot in the PT and to a study of the variation of hoop strains accumulated in the pressure tube at the time of failure. These two strain quantities will be influenced by parameters such as internal pressure, rate of heating (which in turn is dependent on decay heat), the amount of coolant in the channel and the variation of temperature around the circumference. Thus, the problem of constructing the failure maps is a study of the variation of local and diametral strain of the pressure tubes with these parameters as variables.
3.0 FAILURE CRITERION

Examination of the fracture surface of pressure tubes which failed at high temperatures due to creep indicates that the local failure strain at the point of failure is almost infinite as the wall thickness has reduced to zero. Also, it has been observed that the pressure tubes invariably contain axial scratches. Based on these observations, a method to predict lower and upper bounds to the average hoop strains at failure has been developed in Reference (5). The pressure tube is considered to have failed when the local strain tends to infinity i.e. the wall thickness reduces to zero. The lower bound criteria assumes that the axial scratches coincide with the hot spot while the upper bound criterion assumes the pressure tube has uniform wall thickness.

In the present analysis, as a practical limiting criterion, the maximum local strain at any point is limited to 100 percent at which point the tube is considered to be failed. The present strain limit of 100 percent local strain is more limiting than the upper bound criterion of Reference (5) as the tube is assumed to have failed even though 33 percent of the original thickness is still remaining as compared to zero thickness assumed in Ref (5). Thus in effect the present analysis accounts for the presence of scratches by a more limiting local strain failure criterion.

4.0 TEMPERATURE PROFILES AROUND PRESSURE TUBE CIRCUMFERENCE

Consider the stratified flow situation as an example for developing non-uniform temperatures on the pressure tube. The geometry of the pressure tube which is partially filled with liquid coolant is shown in Figure 1. The temperature on the wetted portion of the pressure tube essentially remains at the coolant saturation temperature ($T_0$). By symmetry considerations, the temperature at the top of the channel will be a maximum at $T_{\text{max}}$.

The variation of temperature around the circumference, for the present study, is represented as

$$ T(\theta) = T_0 + (T_{\text{max}} - T_0) \left(1 - \frac{\theta}{\theta_1}\right)^n $$

for $0 < \theta < \theta_1$ (1)

This representation results in a linear variation for $n = 1$, in a parabolic variation for $n = 2$ and a cubic variation for $n = 3$.

As a limiting case, which represents a very peaked yet smooth variation of temperature, a cosine variation of temperature is also considered by representing the temperature variation as

$$ T(\theta) = T_0 + \left(\frac{T_{\text{max}} - T_0}{2}\right) \left[1 + \cos\left(\frac{\pi \theta}{\theta_1}\right)\right] $$

These temperature variations are shown schematically in Figure 2. Thus, in the present analysis four temperature profiles have been considered i.e., linear ($n=1$), parabolic ($n=2$), cubic ($n=3$) and cosine variation. Currently models are being developed to analytically predict the actual temperature distribution in the pressure tube, including the effects of circumferential conduction in the PT, radiation from the exposed fuel and convection from the steam. The predictions from these models, when available, will be used to verify the failure maps.

4.1 Pressure Tube Temperature Transients Considered

In the type of accidents considered here in, the maximum temperature in the portion of the pressure tube adjacent to the exposed fuel bundle is assumed to increase exponentially to a steady state value ($T_{\infty}$) and is represented as:

$$ T_{\text{max}} = T_0 + (T_{\infty} - T_0) (1 - e^{-t/\tau}) $$

where $T_{\infty}$ - Asymptotic steady state temperature
$t$ - Time in seconds
$\tau$ - A parameter representing rate of heating (a time constant)

With this representation of temperature transient the initial heating rate $\frac{dT}{dt}|_{t=0}$ is equal to $\frac{\tau}{t(t-\tau)}\pi$. The heating rate ($\frac{dT}{dt}$) reduces by a factor of 2 in about every 0.71 seconds. By varying the parameters $T_{\infty}$, $\tau$, $n$ and $\theta_1$ any rate of heating can be simulated in the analysis.
Variation of temperature profile within n

FIGURE 2

With the above representation of temperature profiles and transients, the main parameters influencing pressure tube integrity are identified as:

- $P$: the internal pressure
- $\theta$: the angle subtended by steam space (a parameter for depth of coolant)
- $T_a$: the asymptotic temperature
- $\alpha$: a parameter defining the rate of heating
- $n$: the shape parameter

The influence of these parameters on the failure temperature ($T_f$) and the average hoop strain at failure ($\epsilon_{\text{ave}}$) will be evaluated using the methodology described below.

The evaluation of the creep rates of the pressure tube due to a known internal pressure and temperature distribution is analytically straightforward. The principal stresses in the pressure tube are the hoop stress and the axial stress which are easily evaluated. The creep constitutive equations are well documented (Reference 3,4). Knowing the stress state and the creep rates at any instant, the total strain accumulated and its distribution can be evaluated as a function of time by integrating in small time steps. A small dedicated computer code (NUHAI) has been developed to perform these calculations which is similar to other codes developed for computation of non-uniform strain in pressure tubes (such as GRAD Reference 4). This computer code (NUHAI) has been validated by comparing its results with those obtained from experiments (Reference 2,7) in which the failure temperature and the strain at failure of biaxially strained PTs are measured in tests with constant linear heating rates. All these validations have been reported in Reference (8).

5.1 Sensitivity of the Main Parameters

To evaluate the significance of the various identified parameters in assessing the pressure tube response, a detailed examination of the failure temperatures and the average strain at the time of failure are obtained for the following variation of parameters:

$$
\begin{align*}
P &= 3-7 \text{ MPa} \\
T_a &= 750-900^\circ \text{C} \\
\alpha &= 20-100 \\
\theta &= 60-120^\circ \text{C} \\
n &= 1, 2 \text{ and } 3 \text{ and cosine variation}
\end{align*}
$$

The failure temperatures and average hoop strain at failure were obtained for the range of parameters considered in equation (4). For example, the results obtained for the case of a PT with a constant internal pressure of 5 MPa with coolant level at half and quarter full levels respectively, are given in Tables 1 to 4. Similar results were obtained for other pressure and coolant levels. From the results of the parametric studies in Tables 1 to 4 it can be observed that:

(a) The failure temperature is independent of temperature distribution and coolant depth, and is dependent only on the local heating rate for a given pressure.

(b) The average hoop strain at failure is not sensitive to the rate of heating ($\alpha_{\text{ramp}}$) and is almost entirely dependent on the temperature distribution at the time of failure (i.e., shape parameters and coolant depth) for a given pressure.

The first observation is evident from the fact that the failure of the PT is a local phenomenon caused by thinning of the pressure tube at the hot spot. The increase in stresses caused by the increase in diameter of the pressure tube is of a lower order of magnitude. The failure temperature is dependent only on the local rate of heating and the internal pressure.

The second observation also becomes evident if one examines the creep strain response of a tube under constant internal pressure due to a transient ramp temperature increase (which is shown schematically in Figure 3). The creep strain increases exponentially close to the failure temperature. The strain accumulated up to the failure temperature in the transient is relatively very small. When this creep response is accounted for, it is evident that the strain in the pressure tube, away from the hot spot, will be negligible when the temperature varies around the circumference. As a result only the extent and the spread of the hot spot will largely determine the average hoop strain at failure.
With these observations, it is clear that the failure temperatures and average strain at failure can be determined independent of each other by considering simplified models. For determining the failure temperature, which is dependent mainly on the rate of heating, only a simplified model of the PT with no variation of temperature around circumference need be considered. The temperature at which the local strains reach 100 percent can be obtained for various rates of heating and internal pressures. The range of parameters (i.e., $T_m$ and $i$) considered, to obtain the failure temperature in Tables 2 and 3, represents an initial heating rate in the range $7.5 < T < 30$°C/sec. To assess the significance of heating rate, constant heating rates which do not decrease with time are considered. The failure temperatures obtained by the present method using constant heating rates are given in Figure 4. If these temperatures are not attained during a transient, the pressure tube is unlikely to have strained to failure. During a transient, when the rate of heating is varying, (as in Equation 3) an estimate of the failure temperature can be obtained based on the average heating rate in the transient.

Similarly, to obtain the average hoop strain at failure ($\epsilon_{\text{ave}}$), based on the second observation, a representative value of heating rate (by selecting suitable $T_m$ and $i$) only need be considered. Only the coolant depth, the shape factor and internal pressure need to be varied to obtain $\epsilon_{\text{ave}}$. However, in the present parametric study, the average hoop strain is obtained by varying $T_m$ and $i$ over the range quoted in equation (4). The values of average hoop strain at failure for various coolant depths are shown in Figures 5 and 6 for all the variations of temperature considered around the circumference.

From these results it is seen that the average hoop strain at failure is much lower than 18 percent for all pressures and coolant depths examined if the temperature variation is assumed to be cosine, linear ($n=1$) or parabolic (i.e., $n=2$). The average hoop strain, for the case of cubic variation of temperature, exceeds 18 percent limit if the coolant depth in the channel is less than about half full (i.e., $\theta_1 = \pi/2$).

6.1 Sensitivity to Failure Strain

In order to assess the sensitivity of the present failure maps to the failure criteria selected, the local strain at failure has been assumed to vary between 75% and 150%. The average strain in the pressure tube has been obtained for these local failure strains for one particular failure map for a quarter full channel. These results given in Figure 7, shows the sensitivity of the results to variations in the failure criterion. For example, at 5 MPa in a one-quarter full channel ($\theta_1 = 120^\circ$), the average strain at failure is 15.4 percent with 75 percent strain failure criterion, compared to 17.7 percent for a 100 percent strain failure criterion. Thus, reducing the failure strain by 25 percent has led to a reduction of 13 percent in the average strain at failure.
The above results indicate that the PT can fall before contacting the CT only if both of the following conditions are met:

(a) The hotspot on the pressure tube achieves the failure temperature associated with the internal pressure and heating rate (Figure 4), and

(b) The temperature profile around the circumference is very steep close to the hotspot (n less than two for almost any water level, or n less than three for channels more than about one quarter full with pressure less than about 5 MPa, as shown in Figures 5, 6).

Conversely, pre-contact rupture is avoided if the peak PT temperature remains below the failure temperature, or if the temperature profile near the hotspot is fairly flat (i.e., n is large). It is unlikely that the pressure tube can attain the failure temperatures in stratified conditions when the water level is relatively high, due to close proximity of a good heat sink (coolant). However, when the coolant level in the channel is low, the temperature at the top of the PT can be high, but the shape of the temperature distribution near the hotspot is expected to be relatively flat, due to radiative heating and low water level in the channel. These conditions are likely to result in a large hoop strain prior to failure.

7.0 APPLICATION TO ACCIDENT ANALYSIS

The above results indicate that the PT can fall before contacting the CT only if both of the following conditions are met:

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7.1 The Case of Fully Voided Channels

As an extreme case of the type of situations considered, the case of a fully voided channel is considered. For the case of a channel completely filled with steam, (i.e., $\theta_i = 180^\circ$), the coolest temperature, $T_o$, may exceed the saturation temperature. Hence all the points on the pressure tube will be heating up but at different rates. This case has been simulated by using NUBAIL with different heatup rates at the top and bottom of the PT (i.e., different values of $T_f$ and $\nu$ in Equation 3), and imposing a shape factor to obtain the desired PT temperatures around the circumference. For each set of temperature conditions (with a constant pressure) using NUBAL, the average PT strain and the top-to-bottom temperature difference at the time of failure have been obtained. These results which show the average circumferential strain at failure as a function of the temperature difference around the circumference are presented in Figure 8. It was again noticed that for a given shape factor and pressure, the average PT strain at failure is independent of the values of $T_f$ and $\nu$ used to obtain the temperature difference. This result is consistent with the observations made earlier.

![Graph](image)

From the results in Figure 8 it is evident that in fully voided channels only the very peaked (parabolic) temperature distribution can result in pre-contact failure of the PT, and even then only at high internal pressure with very large temperature differences. This result occurs because in fully voided channels, virtually the whole of the PT heats up. Thus, only a steep circumferential temperature profile, near the hotspot on the PT, can lead to pre-contact rupture. The results in Figure 8 indicate that fully voided channels are not prone to pre-contact rupture as a result of the circumferential temperature profiles considered.

8.0 CONCLUSIONS

The present parametric study has indicated that the pressure tube integrity under non-uniform temperature conditions can be split into two issues:

(a) does the PT achieve high enough temperature at any point to cause rapid creep strain, and

(b) what is the temperature distribution around the PT (as determined by coolant depth and shape factor)?

The first issue identifies the possibility for failure and this can be identified as the necessary condition. The second issue defines the average hoop strain at failure and this identifies the situation where failure of the PT before contact with the CT is possible.

Based on this study it is concluded that the temperature at which the PT fails by creep straining is a local phenomenon. It can be evaluated separately without considering the circumferential temperature variation. Similarly the average hoop strain at failure is shown to be independent of the rate of heating.

9.0 REFERENCES

(3) Shewfelt, R.S.W. and Godin, D.P., "Ballooning of Thin Walled Tubes With Circumferential temperature Variation", AECL. Report 8317. (To be published).
(4) Shewfelt, R.S.W. and Godin, P., "Verification Tests for GRAP, a Computer Program to Predict the Non-Uniform Deformation and Failure of Zr-2.5% Nb Pressure Tubes During a Postulated LOCA", AECL. Report B384. March 1985.
(5) Shewfelt, R.S.W., "Failure Maps for Internally Pressurized Zr-2.5% Nb Pressure Tubes With Circumferential Temperature Variations", WEIR, AECL. Report 8399, January 1986.


### TABLE 1
Variation of Failure Temperature (\(T_f^\circ K\)) with \(T_m\) and \(\theta\)
Pressure \(P = 5\) MPa, \(\theta_f = 90^\circ\)

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### TABLE 2
Variation of Failure Temperature (\(T_f^\circ K\)) with \(T_m\) and \(\theta\)
Pressure \(P = 5\) MPa, \(\theta_f = 120^\circ\)

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### TABLE 3
Variation of Average Strain at Failure with $T_m$ and $T_x$, $P = 5$ MPa, $\theta_1 = 90^\circ$

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Average 3% Ave 10% Ave 16.2% Ave 6.4%

### TABLE 4
Variation of Average Strain at Failure with $T_m$ and $\delta$
$P = 5$ MPa, $\theta_1 = 120^\circ$

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Average 3.8% Ave 13.1% Ave 18.3% Ave 8.6%
Post-test metallographic examination of bundle cross sections of a 19-element modified CANDU fuel bundle was carried out. The bundle, HTBS-004, had been subjected to a severe temperature excursion to 1900°C in superheated steam. For this study, quantitative image analysis, Auger analysis and SEM-EDX techniques were applied. A significantly large quantity of molten (Zr, U, O) alloy was relocated in the bundle section 50 mm from the upstream end, whereas the 377-mm section showed little relocated material except at the inner element junctions. These variations in the molten material generation and relocation have been correlated with the corresponding axial and radial variations in the heatup rates.

INTRODUCTION

During a severe loss-of-coolant accident in a CANDU* reactor, rapid temperature escalation can lead to severe bundle deformation and melting of the Zircaloy-4 cladding. These phenomena are significantly affected by cladding oxidation and production of layers of ZrO and oxygen-saturated a-Zr(O) of much higher mechanical strength and melting temperature than the initial Zircaloy-4 material.

To develop a better understanding of the interactive phenomena involved in the generation and relocation of molten material in a fuel bundle, a series of high-temperature, bundle-sag (HTBS) tests were performed on unirradiated fuel bundles. These tests were conducted at the Westinghouse Canada (WECAN) test facility as part of a CANDEV (CANDU Development) program on severe fuel damage studies. In the first two tests (HTBS-001 and HTBS-002), a 37-element fuel bundle was subjected to high-temperature transients to at least 1700°C in shrouds conforming to pressure tube geometries (1,2). Post-test metallographic examinations (3-5) of cross sections from these bundles were carried out to compare the nature of Zircaloy-4/UO2 interaction and molten material relocation in different bundle/element locations.

In the present test, HTBS-004, a higher cladding temperature of 1900°C was achieved by using a 19-element bundle (6) constructed by removing the 18 outer elements from a standard 37-element Bruce bundle.

This investigation focuses on metallographic examination of two bundle cross sections, one 50 mm and the other 377 mm from the upstream (torch) end of the bundle HTBS-004. Optical and electron metallography using scanning electron microscopy (SEM), energy dispersive X-ray (EDX) and scanning Auger microscopic techniques were used to determine the nature of steam/Zircaloy/UO2 interaction. Quantitative image analysis was also applied to examine the amount of relocated material in different bundle locations. The main objective of this study was to determine the influence of a high cladding temperature and varying temperature ramp rates on the nature and extent of molten material relocation in different axial and radial bundle locations.

EXPERIMENTAL PROCEDURES

Heating of Bundle HTBS-004

Bundle HTBS-004 was heated by flowing superheated steam (~2000°C), generated by an oxy-hydrogen torch, along the bundle. Plant steam (170°C maximum) was mixed with the torch steam to control the temperature ramp and steam flow rates. Temperature measurements were made by an optical pyrometer, thermocouples and 'melt' wires (of known melting points) inserted in selected fuel pellets at different axial and radial bundle locations (6).

After preheating the bundle for about 500 s at 1000°C to 1200°C, plant-steam flow was reduced, and the superheated steam (1800°C to 2000°C) was passed along the bundle for 300 s at a flow rate of 10.5 g/s. When the temperature stabilized, indicating the end of Zr + H2O reaction, the steam flow from the torch was shut off and the plant steam flow was maintained at 1.5 g/s to cool down the bundle.

The cladding temperature reached at least 1900°C along the whole length of the bundle (6,7). However, the heatup rates varied over a wide range from 26°C/s (at the 50-mm section) to 9°C/s at the 377-mm section. Since the bottom elements were in contact with the shroud, they experienced a uniformly low heatup rate of 6 to 7°C/s along the entire bundle length (see Figure 1).

Metallographic Examination

For post-test examination, the bundle was potted in epoxy and sectioned radially at specified distances from the upstream end. Two cross sections, one 50 mm and the other 377 mm from the upstream end, were subjected to detailed metallographic, SEM and Auger examinations.

After mechanical polishing and macroscopic examination of the fuel bundle sections, pieces for metallographic studies were cut from several locations - top, centre and bottom regions of the
These samples had clusters of two to four elements and have been designated by their element numbers using the conventional CANDU bundle numbering system.

The metallographic samples were impregnated with epoxy, ground and polished by the usual metallographic procedures, and finally attack-polished and etched in a 0.5 vol% HF solution in water.

**Quantitative Metallography**

Quantitative image analysis was performed on a photomacrograph of the 50-mm section of the bundle, to determine the areas of relocated material, the residual flow subchannels that were partially blocked by the relocated material and the residual UO$_2$ pellets after surface interaction with the attached cladding. In this study a LEITZ TAS PLUS automatic image-analysing system at the Department of Mechanical Engineering of the University of Manitoba was used. A television camera, focussed onto the photomacrograph of the bundle section, scanned the selected features within a circular area defined by a built-in mask. Before scanning, a video display of any selected feature at a time (such as the subchannel) was made by means of careful adjustments, to match the shades of grey by the so-called 'erosion' and 'dilation' mechanisms. Care was taken to ensure that the lighting during photomacrography was uniform over the entire area, to completely isolate one feature from the other. A computer plot was made of each particular feature scanned. The video scale was calibrated and the selected area was computed by the system.

**RESULTS AND DISCUSSION**

**Bundle Sections: Macroscopic Examination**

Figures 2A and 2B are macrographs of the two bundle cross sections at 50 mm and 377 mm from the upstream end. At both these locations, the bundle had deformed to a noncircular slumped type geometry involving downward and sideways movement of the elements leading to extensive interelement contact. These contact points had profound effect on molten material generation and relocation. Also shown in the macrographs are (i) the network of residual (oxidized) cladding, (ii) the UO$_2$ pellets with a part of their surface uncovered in some interior elements, (iii) the relocated material, and (iv) the residual flow subchannels.

The 50-mm and 377-mm sections (from the upstream hot end) experienced a large difference in heating rates of 26°C/s and 9°C/s as shown in Figure 1. This figure was constructed from the measured and interpolated bundle temperature data from test HTBS-004 presented by Wadsworth (7). The extent of cladding oxidation at the 377-mm section was therefore far greater and the amount of molten material relocated was far less than at the 50-mm section.
Figure 2A also shows that the extent of relocated material in the bottom 120° sector of the 50-mm bundle section was significantly greater than in the upper sectors. The sites favoured for molten material relocation are (i) the pellet/cladding gaps, (ii) the inter-element spaces, and (iii) the subchannels.

Relocation in the Bottom Subchannels.

One notable feature of the 50-mm section is that a large amount of molten material has relocated in the bottom subchannels bounded by the zirconia shroud. As shown later, this relocated material has similar composition to that of the material relocated in other locations such as the inter-element contact points or pellet/cladding interface regions.

The amount of molten material available locally from molten cladding and Zircaloy/Un, interaction is too small to account for the large deposits on the shroud. A potential source of this excess material is the end caps at the upstream (hot) end of the bundle. At the upstream end where the superheated steam first impinges on the bundle end plates and end caps, the heatup rate is high and is estimated (see Figure 1) to be \( \frac{300}{6} \) °C/s. At this heatup rate according to FRDM (Full Range Oxidation Model) Code (9) predictions (9), about 75% of the Zircaloy-4 clad cap thickness must have been \( \beta \)-Zircaloy-4 (with 0.13 wt% oxygen) just before melting. It is known that molten Zircaloy with oxygen content less than 1.5 wt% will have poor wettability with Un. (9). This molten material could thus freely move downward and horizontally through the subchannel and deposit downstream where a trough appears to have formed due to the element bow caused by axial restraint applied to the bundle. At the 50-mm section, the top and centre elements were found to have bowed downward by \( \pm 5 \) mm from their normal horizontal positions (6).

Inter-element Contacts

Extensive element contacts have been established at both cross sections due to sagging and settling and possibly bowing of the fuel elements (see Figures 2A and 2B). This happens at high temperature when the strength of the end-plate support and of the cladding becomes too low to resist element creepdown under its own weight. It has been reported (7) that, even at the midplane, the spacers slipped apart to allow the adjacent elements to contact each other.

At the points of contact, solid-state bonding of the cladding, even though oxidized, has taken place. This is clearly indicated in Figure 1 by the deformed \( ZrO_2 \) grain boundary structure near such a junction.

Depending on the mode of settling of the elements, several element cluster geometries with 2 - 4 elements are formed. Within the cluster regions, usually bounded by the adjacent cladding joints, much higher temperatures are likely due to reduced radiation losses. Furthermore, these subchannels are less accessible to steam flow and offer favourable sites for the cladding and the Zircaloy/Un, reaction products to melt away and partly uncover the Un, pellets (see Figure 2). The molten material is also drawn into the narrow wedge-shaped gaps (9) by capillary forces around the interelement contacts which play a crucial role in the relocation of molten material during high temperature transients.

Since the amount of relocated material is proportional to the number of element contacts, a knowledge of the number of contacts around an element circumference is necessary to evaluate relocation behaviour. The number of contacts do vary with the settled element geometry, which seems to be also linked with the heatup rate. For example, at the 50-mm cross section (Figure 2A) the outer elements (19-30) have three contact points each (ignoring the shroud) and develop alternate element clusters of triangular and quadrangular geometries. On the other hand, the inner elements (31-37) each have five contact points. Comparatively fewer contact points developed at the 377-mm section. In this section, the per element contact numbers are 4 for the top outer elements, 2-3 for the side and bottom outer elements, and 3-4 for the inner elements (31-36). Thus a significant reduction of 20 to 60% in the number of element contacts occurred for the inner and outer elements of the 377-mm section, compared with the 50-mm section. This shows the effect of delayed bundle slumping caused by a slow heatup rate.

Cladding Oxidation

The extent of cladding oxidation differed widely not only between the two bundle sections but also from one location to another in the same cross section. Oxide thickness also varied circumferentially from outer to inner locations of the same elements.

As shown in Figure 4, the oxide structure was composed of external layers of \( ZrO_2 \) probably formed as oxides of tetragonal and cubic structures (10). The outer oxide layer was about 0.120 mm thick, had a fine columnar grain structure and flaked off from the substrate oxide, which had a coarse columnar grain structure. An interior \( \beta \)-\( Zr \) phase was also observed in the 377-mm section. Within this layer, a dispersed phase of \( (\gamma, \gamma') \) with a low melting...
temperature, formed by Zircaloy/\ce{UO2} interaction, is also present and seems to facilitate melting of the layer. The residual \(\alpha\)-Zr(O) layer was probably saturated in oxygen content and had a melting temperature (1950°C) higher than the test temperature.

### \(\ce{ZrO2}\) Thickness

At the 50-mm section, the residual cladding was fully oxidized to \(\ce{ZrO2}\). As the unoxidized inner layer has presumably melted away the residual oxide layer is thinner than the original cladding thickness of 0.42 mm (nominal), which on complete oxidation will increase to 0.65 mm. On the exterior side of the outer elements, the average oxide thickness (Table 1) varied from 0.26 mm for the top elements to 0.45 mm for the bottom elements. An average intermediate oxide layer thickness of 0.32 mm was observed for the side outer elements. This increasing oxide thickness from top to bottom can be ascribed to the decreasing heatup rate from 26°C/s to 7°C/s (Figure 1).

For the inner elements and also the interior sides of the outer elements, the residual cladding (\(\ce{ZrO2}\)) thickness was much less, 0.14 to 0.26 mm. This is expected from a higher heatup rate at these locations. Calculation using FROM Code predicts a composite \(\ce{ZrO2} + \alpha\)-Zr(O) thickness of 0.14 mm for cladding heated in steam at 26°C/s up to 1760°C (9).

At the 377-mm section, the oxidized outer cladding structure was composed of both \(\ce{ZrO2}\) and \(\alpha\)-Zr(O) layers. However, for the interior element regions, only the \(\ce{ZrO2}\) layer was seen and the inner layer of \(\alpha\)-Zr(O) has again presumably melted away. Examples of the average \(\ce{ZrO2}\) layer thickness (Table 1) show the variations for the outer elements from 0.36 to 0.45 mm as compared to 0.32 to 0.51 mm for the inner elements and also for inner sides of the outer elements. The oxide thickness for elements 26 and 28 was the largest (0.51 mm).

### Quantitative Image Analysis of the Relocated Material

Figure 5 shows that the bundle cross-section at 50 mm is composed of four clearly identifiable regions, which have different shades of grey colour and are thereby amenable to quantitative image analysis. The regions are (1) the residual subchannels (black), (2) the relocated material (dark grey), (3) the unreacted \(\ce{UO2}\) pellet (light grey) and (4) the residual cladding.

Since the amount of relocated material was comparatively larger at the bottom subchannels, two specific areas enclosed within the scissored circles (A) and (B) (Figure 5) were selected to provide limited comparison of the relocation behaviour in the central and bottom regions of the bundle.

---

**Table 1: Zirconium Oxide Thickness Variation at Different Bundle Locations**

<table>
<thead>
<tr>
<th>Axial Distance</th>
<th>Element Location</th>
<th>Heatup Rate °C/s</th>
<th>Average Oxide Thickness mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>50 mm</td>
<td>Top Outer - 30</td>
<td>1</td>
<td>0.26</td>
</tr>
<tr>
<td></td>
<td>Side Outer - 22</td>
<td>1</td>
<td>0.32</td>
</tr>
<tr>
<td></td>
<td>Bottom Outer - 25</td>
<td>1</td>
<td>0.26</td>
</tr>
<tr>
<td></td>
<td>Inner - 32,33,36</td>
<td>-</td>
<td>0.14 - 0.25</td>
</tr>
<tr>
<td>377 mm</td>
<td>Top Outer - 30</td>
<td>1</td>
<td>0.32</td>
</tr>
<tr>
<td></td>
<td>Side Outer - 22</td>
<td>1</td>
<td>0.44</td>
</tr>
<tr>
<td></td>
<td>Side Outer - 28</td>
<td>1</td>
<td>0.48</td>
</tr>
<tr>
<td></td>
<td>Bottom Outer - 25</td>
<td>1</td>
<td>0.45</td>
</tr>
<tr>
<td></td>
<td>Inner - 31-37</td>
<td>-</td>
<td>0.23 - 0.50</td>
</tr>
</tbody>
</table>

* From the Bundle Upstream End
** Locations (I) and (II) refer to the Outer and Inner Cladding Regions of the Element
*** Element number — see Figure 2

---

* Average of 4-8 thickness measurements
FIGURE 5: Bundle Cross Section (At 50 mm) Showing the Centre Region (A) and Bottom Region (B), Which Were Scanned for Quantitative Image Analysis. Also shown are the different constituents, (1) the residual subchannels (black), (2) the relocated material (dark grey), (3) the residual U\textsubscript{2}O\textsubscript{3} pellets (light grey) and (4) the residual cladding (white).

Figure 6 shows the digitized computer images of the areas corresponding to three specific features: the U\textsubscript{2}O\textsubscript{3} pellet (A), the relocated material (B) and the subchannel (C) for the two selected locations at the centre and the bottom regions. Note that the computer printout gives a reverse image of that in the photomacrograph (Figure 5). The computed areas for each of the above features are given in Table 2.

Table 2

<table>
<thead>
<tr>
<th>Constituents</th>
<th>Area, cm\textsuperscript{2}</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Centre Location</td>
</tr>
<tr>
<td>Pellets</td>
<td>3.47</td>
</tr>
<tr>
<td>Relocated Material</td>
<td>1.02</td>
</tr>
<tr>
<td>Subchannel</td>
<td>1.48</td>
</tr>
</tbody>
</table>

* Limits due to erroneously scanned cladding and relocated material (Figure 6A - Bottom)

FIGURE 6: Computer Printouts of the Individual Areas of the Pellets (A), the Relocated Material (B) and the Residual Subchannels (C) at the Centre and Bottom Regions of the Bundle. Errorously included in the U\textsubscript{2}O\textsubscript{3} area for the bottom are some cladding and relocated materials marked X and Y respectively.

U\textsubscript{2}O\textsubscript{3} Pellet

As shown in Figure 6A the cracks within the U\textsubscript{2}O\textsubscript{3} pellet have been eliminated from the shaded pellet area and the computed area can thus be compared with that of the original pellet to estimate the loss of U\textsubscript{2}O\textsubscript{3} pellet cross section due to interaction with the Zircaloy cladding. This can be done by scanning full cross sections of several fuel pellets. In Figure 6A (Bottom), part of the cladding 'X' and the relocated material in the subchannel region 'Y' appear along with the U\textsubscript{2}O\textsubscript{3} pellet and the computed pellet area is thus slightly in error. This problem did not occur while scanning the centre region and it can be corrected by improved sample preparation, by uniform lighting during photomacrography and by adjusting the 'erosion' and 'dilation' controls.

Subchannel

As shown in Figure 5, relocated material occupies a varying fraction of the subchannel area in different bundle locations. The residual subchannel area is expected to show complimentary variations to the relocated material. The residual subchannel area in the bottom section (Figure 6C) is 48% less than the corresponding area in the centre region. Sagging of the fuel elements at high temperature contributed to a greater number of interelement contacts and more reduction of the subchannel area.
at the hottn sector as compared with the centre and top sectors. Also, in this test (HTBS-004), the effect of axial restraint applied during heating of the bundle was predominant and it caused the handle to buckle in a wavy shape, creating a trough around this region as stated earlier.

**Relocated Material**

The Zircaloy/UO2 interaction leads to formation of molten (Zr, U, O)-alloy, which flows and relocates between the adjacent elements as shown in Figures 6B. Some of this material also relocated in the subchannel region and at the pellet/cladding interfaces. The amount of relocated material in the bottom subchannel (Figure 6B) is about 50% greater than in the corresponding central subchannels, which must have experienced the highest heatup rate.

**NATURE OF THE RELOCATED MOLTEN MATERIAL**

In the following sections the nature of molten material, relocated in different regions of the bundle, are described. The relocated material and their microconstituents have been identified by SEM-EDX and Auger analyses.

**Element Junction 26/27/35 of the 50-mm Section**

The nature of molten material relocation is clearly shown in a magnified view (Figure 7) of the three-element junction 26/27/35.

Evidently, a significant amount of molten material has relocated into the bottom subchannel between the zirconia shroud and the elements 26 and 27. Similar relocation was seen at the other bottom subchannels below the element junction 26/25 and 25/24 (Figure 2A). In addition to subchannel region (A), the other sites to which the molten material has relocated are the pellet/cladding gap (B) and the interelement spaces (C). In several locations the oxidized cladding (D) is intact sandwiched between the relocated materials (A) and (B). The relocated material had probably reached the equilibrium oxygen concentration and hence was stable in contact with ZrO2.

Auger Analysis was carried out to determine the O, Zr and U concentrations of the relocated material in three different regions, the bottom subchannel region (A), the pellet/cladding gap (B) and the interelement spaces (C). The subchannel material (A) appeared to consist of two regions - a dark region with slightly more Zr (24 at%) and less U (10 at%) than the light region (20 at% Zr, 14 at% U). Both regions contained similar amounts of oxygen (66 at%). The relocated materials at the pellet/cladding gap (B) as well as on the outside of the cladding, had close to it, contained approximately 64 at% O, 23 at% Zr and 13 at% U. The composition of material in another similar location C2 (between elements 26 and 27) contained 65 at% O, 26 at% Zr, and 10 at% U. The composition of material in another similar location C2 (between elements 27 and 35) was slightly different - 61 at% O, 26 at% Zr and 13 at% U. Thus one can conclude that the relocated materials in different locations are basically of similar composition (60-65 at% O, 20-26 at% Zr and 10-15 at% U) leading to a (Zr, U)O2-x-type ceramic phase.

**Element Junction 25/26/34 of the 50-mm Section**

Another example of molten material relocation at a bottom subchannel of the 50-mm section is shown in Figure 8, which is a secondary electron micrograph of element junction 25/26/34. In this micrograph the UO2 pellets (A) are fully enveloped by the relocated material (B), which initially filled the pellet/cladding gap and then advanced to fill the triangular subchannel space from all sides. This molten material shows good wettability with the UO2. Part of the residual cladding material (C) is still retained. The X-ray energy spectrum (Figure 8B) indicates that the material is a (Zr, U) alloy which contains O but not detected by SEM-EDX. A similar X-ray energy spectrum was observed for the material deposited between the shroud and the bottom elements 25/26 (Figure 9B).
FIGURE 8: A Secondary Electron Micrograph of the Element Junction 25/26/34 (50-mm Section) Showing the UO₂ Pellets (A), the Zr, U, O) Relocated Material (B) and the Cladding (C). The X-ray energy spectrum (B) of the relocated material is also shown.

Element Junction 23/33 of the 50-mm Section

A secondary electron image of another element junction 23/33 of the 50-mm section is displayed in Figure 10. It is evident that in both elements the molten material has flowed from the hot core of the element junction 'C' into the pellet/cladding gaps 'G'. The residual claddings (ZrO₂) of both the elements are bonded into a V-shaped joint (J) and the molten material has relocated on both inner and outer sides of the oxidized claddings.

From this micrograph, if the effects of cooling are ignored, the contact angles of the relocated material were determined to be ~40° with the UO₂ and even less (~30°) with the ZrO₂. Due to such low (<90°) contact angles the alloy can easily wet both UO₂ and ZrO₂. These low contact angles indicate that the relocated material should have a high oxygen content (11). In a recent analysis (9) of capillary forces, the contact angle for molten Zircaloy with ZrO₂ is assumed as equal to that with ZrO₂, which seems conservative in view of the present data. But one should bear in mind that the oxygen content of the molten material keeps changing during the relocation process.
Element Junction 25/26 of the 377-mm Section

Figure 11 is a secondary electron micrograph of the bottom element junction 25/26 of the 377-mm section. Being close to the zirconia shroud the elements were heated at the slowest rate (6.6°C/s). Thus a large part of the cladding (A) was oxidized to an average oxide thickness of 0.50 mm.

In another region of the cladding near the junction 25/26 (see Figure 12), a Sn-rich (Zr-Sn) phase (white, X) and a (Zr-Sn-Fe-Cr) phase (Y) are dispersed in the α-Zr(O) matrix at the inner cladding region. The X-ray energy spectra for these phases are also shown. Sn (1.5 wt%), Fe (0.2%) and Cr (0.12%) are the alloying elements of Zircaloy-4 and are usually found (5, 12) segregated at the oxide front as it moves inward during external steam oxidation of the cladding material. This type of segregation is seen only when the oxidation rate is slow.

CONCLUSIONS

From a post-test metallographic examination of the two bundle cross sections (50 mm and 377 mm from the upstream end) of the WECA test bundle HTBS-004 the following conclusions were made.

(1) Examination of the two bundle cross sections indicates that the number of interelement contacts depends on temperature ramp rate.

(2) Molten material relocated in much greater quantities at the 50-mm section than the 377-mm section. Relocation at outer elements of the latter section was minimal, as due to slow heatup rates, most of the cladding was oxidized to ZrO₂ + α-Zr(O). At the 377-mm section, melting and relocation of the cladding material occurred mainly at the interior element junctions. By contrast, significant amounts of molten material relocated all over the 50-mm cross section. The favoured sites of relocation are the cladding/pellet gap, the space around the interelement contacts, and the subchannel regions.

(3) A larger quantity of molten material was found relocated at the shroud near the bottom subchannels in the 50-mm section. This excess material originated from molten end-caps which flowed possibly downward through the subchannels to the bottom location which coincided with a trough created by element-bow under applied axial restraints.

FIGURE 11: A Secondary Electron Micrograph of the Element Junction 25/26 (377-mm Section), Showing the Fully Oxidized Cladding (A) and the Cladding Junction.

FIGURE 12: A Backscattered Electron Micrograph of A Cladding/UO₂ Interface Region Near the Element Junction 25/26 Showing the Zr-Sn Phase (X) and the (Zr-Sn-Fe-Cr) Phase (Y) Segregations. The corresponding X-ray energy spectra (X and Y) are also shown.
The relocated material in different regions such as the subchannel, the pellet/cladding gap and the interelement region, all had high oxygen concentration and basically similar composition with 60-65 at% O, 20-26 at% Zr and 10-14 at% U.

ACKNOWLEDGEMENTS

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REFERENCES


(3) P.J. FEHRENBACH, R.D. DAVIDSON and J.A. SCOBERG, Unpublished Information.

(4) R.D. DAVIDSON and P.J. FEHRENBACH, Unpublished Information.


(7) S. WADSWORTH, Private Communication.


ABSTRACT

Atomic Energy of Canada Limited is designing a tomographic scanner for the post-irradiation examination of nuclear fuel assemblies. The requirements for the facility are briefly reviewed and a conceptual design is presented. Demonstration scans of sections of non-active fuel assemblies have been performed to verify the design parameters.

INTRODUCTION

Atomic Energy of Canada Limited - Chalk River is currently designing a tomographic scanner for the post-irradiation examination (PIE) of nuclear fuel assemblies. Transmission tomography is a non-intrusive non-destructive testing technique for generating quantitative cross-sectional attenuation coefficient maps of objects. As such, it is a valuable technique for characterizing the distribution of previously molten uranium-zirconium-oxygen alloys within fuel assemblies subjected to severe accident simulation tests, such as those to be performed in the CRNL Blowdown Test Facility (BTF). It can also be used for selecting regions within an assembly for more conventional metallographic PIE. In this paper, the requirements for the Chalk River facility are briefly reviewed and a conceptual design is presented. Images from proof-of-principle scans are used to show the usefulness of the data that can be obtained from tomographic images.

REQUIREMENTS

A feasibility study was carried out to define the requirements for the scanner and to determine the technical and economic feasibility of using neutrons, gamma-rays and/or X-rays as the source of radiation. It was determined that the scanner must be:

- capable of imaging CRNL BTF fuel strings (50 cm long by 7.6 cm in diameter - see Figures 1 and 2) and USNRC/Battelle LWR fuel strings (37 cm long by 10.3 cm in diameter - see Figures 3 and 4),
- able to distinguish Zr, ZrO₂, U₃O₈, previously molten uranium-zirconium-oxygen alloys, stainless steel and inconel,
- able to resolve and measure quantitatively features (e.g., voids and resolidified material) that are 2 mm on a side and greater, and
- capable of collecting emission tomographic images of both fuel assemblies (secondary importance).

Following a thorough survey of available radiation sources, it was concluded that a transmission tomographic scanner incorporating a Co-Bu source and banks of scintillation detectors was the most cost-effective alternative. Linacs were rejected due to their high cost and tomography based on epithermal neutrons was rejected due to the lack of a suitable source of such neutrons at CRNL.
CONCEPTUAL DESIGN

The proposed system will be located in the NRU fuel rod bay at CANL (see Figure 5). It will be capable of handling fuel assemblies up to 350 cm in length by 12 cm in diameter. This spans the range from standard CANDU fuel to some LWR geometries. The system will be based on a second generation rotate-rotate CT scanner design (1) and will be designed to have a spatial resolution of 1 mm full-width at half-maximum (FWHM) both in-plane and axially. The scanner will utilize a $1.1 \times 10^{12}$ Bq (30 GBq) $^{60}$Co-60 source and $^{125}$I gadolinium orthosilicate (GSO) scintillation detectors. The source and detectors will be mounted on a carriage that can be moved relative to the vertically-held fuel assembly. With 12 detectors, it will take ~1 hour to image each slice of the NRU assembly and up to 8 hours for larger assemblies. In both cases, the contrast resolution achieved in each image will be 1.7% (relative to $^{235}$U). All aspects of the data acquisition and scanner movement will be computer-controlled. The same scanner will also be used to generate digital radiographs and gross emission radiographs of the fuel assemblies over the complete 350 cm length by 12 cm width. Emission tomography will also be possible for some isotopes.

PROOF-OF-PRINCIPLE EXPERIMENTS

To demonstrate the efficacy of the proposed design, proof-of-principle tomographic scans were performed on a section of fuel cut from bundle HTBS-014 used in the Westinghouse Canada High Temperature Bundle Sag (HTBS) tests (2,3). This section was selected because it contained features typical of those expected in the HTBS assemblies. As shown in Figures 6 and 7, the...
specimen consisted of 19 fuel elements surrounded by ZrO₂ insulation and encapsulated in plastic. Features of interest include the previously molten material near the bottom of the section, cracks and holes in various fuel pencils, ballooned and ruptured fuel sheaths and several holes in the plastic.

The tomographic data were generated using a general-purpose translate-rotate tomographic scanner operated by the Radiatiion Engineering Branch at CRNL (4). The spatial resolution was set to be 1 mm FWHM while the contrast (density) resolution was about 0.010 cm⁻³ (1.5% relative to U₃O₈). Three scans were made through the sample at axial locations approximately one-quarter, one-half and three-quarters the way along the section. The resulting tomographic images are shown in Figures 8, 9 and 10. All images are oriented as if one were looking at the section from the same orientation as shown in Figure 6. In the images, high density regions (U₃O₈ ≈ 11.6 g·cm⁻³) are displayed in white while low density areas are displayed in black. Gray scales are shown to the right of each image. These black and white images show less detail than equivalent colour images due to
the reduced sensitivity of the human eye to shades of gray as compared to colour and due to the non-uniform response of the film used to photograph these images. The gray level assignment used for the images has been selected to show as much detail as possible. All image analysis, though, was performed with the aid of a colour look-up table.

Linear attenuation coefficients (related to material density) were measured from the images for the major materials in the section. The agreement was excellent. In particular, the value for $U_0$ was measured to be $0.066 \pm 0.007 \text{ cm}^{-1}$ ($\rho = 10.7 \pm 0.1 \text{ g cm}^{-3}$) in agreement with the theoretical value of $0.060 \text{ cm}^{-1}$ ($\rho = 10.6 \text{ g cm}^{-3}$).

Each image shows previously molten material bridging the gaps between a number of the fuel elements. Furthermore, larger amounts of this same material are located in several of the sub channels. An analysis of the attenuation coefficients for this material yields values ranging from $0.12 \pm 0.01$ to $0.39 \pm 0.01 \text{ cm}^{-1}$ ($\rho = 5.1 \pm 0.2 \text{ g cm}^{-3}$). These values lie between those for $\text{ZrO}_2$ ($0.30 \text{ cm}^{-1}$) and $U_0$ ($0.66 \text{ cm}^{-1}$) and are consistent with the formation of $U/\text{Zr}/\text{O}$ alloys. There is also a distinct indication of an interaction having taken place between the resolidified material at the bottom of the lower slice and the $\text{ZrO}_2$ insulation.

The images indicate that most pellets contain cracks that are considerably smaller than the spatial resolution. A close examination of the surfaces of the section confirms this observation. Using image analysis techniques, the width of one such crack was determined to be $69 \pm 4 \mu\text{m}$ wide. Using vernier scales the same crack was measured at the surface of the section ($4.2 \text{ mm}$ away from the image slice) to be $100 \pm 50 \mu\text{m}$ wide. The agreement is good considering the large distance between the two measurement points. Holes drilled in several pellets to contain melt wires were measured from the images to have a diameter of $1.1 \pm 0.1 \text{ mm}$ as compared to their actual diameter of $0.8 \text{ mm}$.

With an appropriate choice of colour look-up table for the display monitor, the difference between oxidized and unoxidized zircaloy and between $\text{ZrU}_2$ and $\text{Zr-U-O}$ alloys resulting from the interaction of $U_0$ with the zircaloy fuel sheath can be displayed visually. Unfortunately, these images do not photograph well in black and white and have not been included in this paper.

CONCLUSION

The requirements for a facility to inspect irradiated fuel from the CANDU HTR and USNRC/Battelle test programs have been reviewed and the results of a feasibility study presented. Demonstration scans performed on a section of the HTS-004 bundle have shown that useful data can be collected at a spatial resolution of $1 \text{ mm FWHM}$ and at a contrast resolution of $1.5\%$ relative to $U_0$. Based on these results, a decision has been made to proceed with the detailed design of the facility.

REFERENCES


SESSION B: REACTOR PHYSICS AND RADIATION

Chairman: A. Harms, McMaster University

Selecting a MAPLE research reactor core for 1-10 MW operation.
H.J. Smith, M-F Roy and P.A. Carlson, AECL WNRE

Assessment of beam tube performance for the MAPLE research reactor.
A.G. Lee, AECL WNRE

Safety and licensing aspects of the MAPLE-X reactor at the Chalk River Nuclear Laboratories.
K.D. Cotnam, AECL CRNL

A non-intrusive neutron method for gadolinium poison concentration monitoring.
E.M.A. Hussein, D. O'Connor and M. Mosher, University of New Brunswick

Comparison of commissioning test results with physics simulations in a CANDU reactor.
A.U. Rehman, M.Z. Farooqui, G.V. Guardalben and A.L. Wight, Ontario Hydro

A review of applications of radiolysis in the adsorbed state.
L.W. Dickson and A. Singh, AECL WNRE
ABSTRACT

The MAPLE class of research reactors is designed so that a single reactor concept can satisfy a wide range of practical applications. This paper reports the results of physics studies performed on a number of potential core configurations fuelled with either 5 w/o or 8 w/o enriched UO₂ or 20 w/o U₀₃Si-Al and assesses the relative merits of each. Recommended core designs are given to maximize the neutron fluxes available for scientific application and isotope production.

1. INTRODUCTION

The MAPLE (Multipurpose Applied Physics Lattice Experimental) class of research reactors is designed so that a single, generic reactor concept can satisfy a wide range of practical applications (1,2,3) e.g.

- neutron beams for basic and applied research using epithermal, thermal or cold neutrons
- production of radioisotopes
- proof testing of fuels and reactor components
- neutron activation of trace materials
- acquiring expertise in specialized nuclear disciplines (e.g. reactor physics)
- training of scientists, engineers and technologists for nuclear programs.

To achieve this range of activities while offering the capability for neutron flux levels that are competitive with specialized reactor designs, economic fuel performance and cost competitiveness, the MAPLE research reactor core structure permits the assembly of a large number of specialized fuel configurations that can be adapted to fit the user’s needs. Hence, while a single reactor concept can be made to accommodate the conflicting requirements of high neutron flux levels and high fuel burnup we do not attempt to satisfy them simultaneously. The balance between the two requirements is specified by the user and a core configuration is chosen to meet the specifications.

Physics studies were performed on cores containing 14-, 19- or 24-fuelled sites. The fuel materials were U₀₂, with uranium enriched to 5 w/o or 8 w/o ²³⁵U in U₀₂ and U₀₃Si-Al with uranium enriched to 20 w/o ²³⁵U in U₀₃Si-Al. Various physics parameters (e.g. reactivity load of fission-product poisons, reactivity burnout rate, etc.) were obtained as functions of core power (1, 3 and 10 MW) for the various core configurations with each fuel type.

This paper reports the results of the physics studies and assesses the relative merits of the various fuels in the different configurations. Recommended core configurations are given to maximize the neutron fluxes available for scientific application and isotope production.

2. REACTOR DESCRIPTION

The structure of the generic MAPLE research reactor is shown schematically in Figure 1. The reactor core design incorporates a compact, modular H₂O-cooled and moderated central region (inner core) surrounded by an annular D₂O region that is penetrated vertically by a regular hexagonal array of H₂O-cooled flow channels (outer core). The D₂O acts as the primary reflector for the inner core. The D₂O tank is sufficiently large so that the outer core is also D₂O reflected. The core-reflector assembly is installed in an open H₂O-filled pool.
dual purpose; reactivity shim for control and shutdown in the inner core, and driver fuel in the outer core. The fuel assemblies may be placed in any site of the inner core and are interchangeable with beryllium or graphite reflector modules which are used to connect the fuelled core segment to the D_2O reflector for cores which do not fuel all 29 sites. A variable number of sites, depending on the core configuration being utilized, are occupied by reactivity shim rods for control and shutdown of the reactor. The reactivity shim rods can be located in any of the sites.

The outer core utilizes H_2O-cooled, D_2O-moderated fuel assemblies in any or all of 14 sites formed in the reflector by the vertical cylindrical zirconium-alloy flow tubes which are located on a hexagonal pitch twice that of the inner core.

Table 1 presents the core specifications.

3. FUEL ELEMENT DESIGN AND FUEL ASSEMBLIES

There are two types of fuel elements considered in this study:

a) Zirconium-alloy clad UO_2 with uranium enriched to either 5 w/o or 8 w/o in ^233U in U.

b) U_3Si-Al dispersion fuel with uranium enriched to 20 w/o in ^233U in U.

The UO_2 element is smaller in size (inner radius = 4.42 mm) than a standard CANDU element to permit the construction of a regular geometric shape that fits inside the reference hexagonal flow tube defined for the U_3Si-Al dispersion assemblies. The UO_2 driver assembly used for this study consisted of 27 such elements on a triangular pitch of 12.4 mm.

The U_3Si-Al dispersion fuel, developed at CRNL, has a composition of 61% U, 24% Si, and 35% Al. The co-extruded cladding has eight fins of rectangular cross section to improve the heat transfer properties. The U_3Si-Al dispersion fuel assembly consists of 36 such elements on a hexagonal pitch of 12 mm, surrounding a solid zirconium-alloy support shaft.

With both types of elements the assemblies used for reactivity shim in the inner core, or outer core drivers consist of 18 elements arranged in two concentric annuli around a solid zirconium-alloy support shaft. Reactivity control is effected by a Hf shroud that slides over the 18-element assemblies. Figure 2 shows cross-sectional diagrams of the fuel elements and the fuel assemblies.

4. MODELLING PROCEDURE

The cell-averaged macroscopic cross sections required to describe the various regions of the reactor were calculated with the widely used multigroup transport code WIMS (4) using the diffusion leakage treatment with transport diffusion coefficients and the discrete ordinates transport solution method. The cross sections for the operating reactor were calculated at the following temperatures: H_2O (pool) at 30°C, H_2O (cooler) and H_2O (reflector) at 40°C, U_3Si-Al fuel at 150°C and UO_2 fuel at 330°C. The D_2O was enriched to 99.8%.

The reactor lattice was modelled using the multi-group, three-dimensional diffusion code 3DDT (5). The spatial mesh was chosen based on previous work and allotted four mesh points per fuel assembly in the X-Y plane and eight mesh points axially. Cases were run with five neutron energy groups (thermal boundary at E_n = 0.625 ev) to reduce computation time.

5. CALCULATIONS

Due to the high ^233U density which is possible with both the UO_2 and U_3Si-Al fuels, the full 29 sites of the inner core are not required for operation in the 1-10 MW power range. The calculations were designed to show the trends of various operational characteristics in cores that contained 14-, 19- or 24-fuelled sites with each fuel type (5 w/o or 8 w/o enriched UO_2

\[ \text{Table 1: Core Specifications for Maple Research Reactor} \]

\begin{tabular}{|c|c|c|}
\hline
\textbf{INNER CORE} & \textbf{OUTER CORE} \\
\hline
\textbf{Shape} & \textbf{Shape} \\
\hline
\textbf{Pitch (mm)} & \textbf{Pitch (mm)} \\
\hline
\textbf{Channel and Assemblies} & \textbf{Channels and Assemblies} \\
\hline
\textbf{Temp.} & \textbf{Temp.} \\
\hline
\textbf{CONTAINMENT VESSEL} & \textbf{CONTAINMENT VESSEL} \\
\hline
\textbf{Reflector} & \textbf{Reflector} \\
\hline
\textbf{Thickness (mm)} & \textbf{Thickness (mm)} \\
\hline
\textbf{Moderator} & \textbf{Moderator} \\
\hline
\textbf{Coolant} & \textbf{Coolant} \\
\hline
\textbf{Temperature (°C)} & \textbf{Temperature (°C)} \\
\hline
\textbf{Cooling Mode and Temperatures} & \textbf{Cooling Mode and Temperatures} \\
\hline
\end{tabular}

46
and 20 w/o enriched U,Si-Al). The unfuelled sites of the inner core were filled with beryllium reflector modules to connect the core to the D,0 reflector. Figure 3 shows the core configurations which were calculated in this study. At operating conditions we determined the reactivity burnout rate (mk/d), the reactivity worth of saturating fission-product poisons, total reactivity worth of the control and shut-off rod systems, power division between driver and reactivity shim assemblies, highest outer element linear power ratings and highest thermal flux levels.

6. RESULTS

Table 2 presents a selection of some of the important operating characteristics determined in this study as functions of the fissile mass in-core. The data for cores fuelled with 5 w/o and 8 w/o UO2 are presented at the 3 MW power level. The data for cores fuelled with U,Si-Al are presented at the 10 MW power level. As discussed below these power ranges seem appropriate for the fuel types in the assemblies defined in this study.

The initial core life, presented in units of full-power-days (FPD) is based on the burnup of a fresh core to keff = 1.0. This number was use as a figure-of-merit to assist ranking of core performance. The estimated core cycle time (FPD), given for the 14-site cores with 5 w/o UO2 and the U,Si-Al fuel, is the equilibrium life expectancy of a core in a fuel management scenario which replaces two driver and two shim assemblies in each fuelling cycle. The core burnup cycles terminated at keff = 1.020 to allow all for unmodelled reactivity loads. The average exit burnup of the driver plus reactivity shim assemblies is presented in MWD/t of heavy elements (H.E.) and percent of initial fissile atoms consumed (XIFAC). The term "reactivity", used in the table, is defined as

\[ \rho = \frac{(k_{\text{eff}} - 1) \times 1000}{k_{\text{eff}}} \]

in units of milli-k (mk).

Figure 4 shows a plot of the peak thermal flux in the D,0 reflector, for the 5 w/o and 8 w/o UO2 fuel in each core configuration, as a function of total core power. The U,Si-Al fuel was calculated at 10 MW only. The values for the U,SI-Al are indicated by triangles whose order in the vertical direction follows the labelling on the curves. The figure illustrates the linear relationship between the flux level and total core power and the non-linear relationship with the number of fuel assemblies. The non-linear relationship is emphasized in Figure 5 by replotting some of the data as a function of fissile mass in-core. The data for the 5 w/o and 8 w/o UO2 fuelled cores are presented at 3 MW. The data for the U,Si-Al fuelled cores are presented at 10 MW.
TABLE 2: PERFORMANCE DATA FOR A VARIETY OF CORES IN THE GENERIC MAPLE RESEARCH REACTOR

Fuelled Sites Configuration | 14 | 19 | 24
<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>2CR/2SD</td>
<td>2CR/3SD</td>
<td>2CR/4SD</td>
<td>5 w/o UO₂ - 3 MW</td>
</tr>
</tbody>
</table>

- Mass $^{235}\text{U}$ (kg) | 5.753 | 7.873 | 9.992 |
- Core Life (FPD) | 251. | 820. | 1334. |
- Estimated Core Cycle Time (FPD) | 111. |
- Exit Burnup - MWD/t H.E. | 13854. |
- - ZIFAC | 27.9 |
- $k_{\text{eff}}$ (fresh) | 1.108 | 1.160 | 1.190 |
- Fission Product Reactivity Load (mk) | 27.4 | 22.5 | 19.2 |
- Burnout Rate (mk/d) | 0.304 | 0.117 | 0.091 |
- Max. Linear Power Rating (kW/m) | 25.9 | 22.3 | 17.9 |

| Shim | 25.9 | 22.3 | 17.9 |
| Driver | 19.1 | 13.6 | 11.6 |
| Max. Thermal Flux in D₂O $(10^{14}\text{n·cm}^{-2}\text{s}^{-1})$ | 0.58 | 0.40 | 0.34 |

- Mass $^{235}\text{U}$ (kg) | 9.205 | 12.597 | 15.987 |
- Core Life (FPD) | 651. | 1908. | 2888. |
- $k_{\text{eff}}$ (fresh) | 1.175 | 1.229 | 1.260 |
- Fission Product Reactivity Load (mk) | -20.9 | -16.5 | -13.7 |
- Burnout Rate (mk/d) | 0.168 | 0.079 | 0.060 |
- Max. Linear Power Rating (kW/m) | 26.2 | 22.6 | 18.1 |
| Shim | 18.7 | 13.4 | 11.5 |
| Max. Thermal Flux in D₂O $(10^{14}\text{n·cm}^{-2}\text{s}^{-1})$ | 0.51 | 0.36 | 0.31 |

| Shim | 20 w/o U₂Si-Al - 10 MW |
| Driver | 18.7 | 13.4 | 11.5 |
| Max. Thermal Flux in D₂O $(10^{14}\text{n·cm}^{-2}\text{s}^{-1})$ | 0.51 | 0.36 | 0.31 |

- Mass $^{235}\text{U}$ (kg) | 5.090 | 6.996 | 8.904 |
- Core Life (FPD) | 140. | 272. | 455. |
- Estimated Core Cycle Time (FPD) | 45. |
- Exit Burnup - MWD/t H.E. | 71453. |
- - ZIFAC | 39.7 |
- $k_{\text{eff}}$ (fresh) | 1.162 | 1.220 | 1.255 |
- Fission Product Reactivity Load (mk) | -41.6 | -36.7 | -33.3 |
- Burnout Rate (mk/d) | 0.699 | 0.528 | 0.373 |
- Max. Linear Power Rating (kW/m) | 72.7 | 63.4 | 50.0 |
| Shim | 43.0 | 31.0 | 25.5 |
| Max. Thermal Flux in D₂O $(10^{14}\text{n·cm}^{-2}\text{s}^{-1})$ | 2.09 | 1.42 | 1.02 |

Figure 4: Peak thermal flux in the D₂O reflector as a function of core power.

Figure 7 presents:

a) the midplane thermal flux ($E_n < 0.625$ eV) and
b) the ratio of resonance flux ($0.625$ eV < $E_n < 9.118$ keV) to thermal flux at the midplane along the long axis of the core assembly (see Figure 1) for a 14-fuelled site U₂Si-Al core operating at 10 MW.

7. DISCUSSION

From the data presented in Figure 4, it is clear that 14-fuelled site cores give significantly better flux performance than either 19- or 24-fuelled site cores at a given power level. In addition, for the fuel assemblies defined in this study, U₂Si-Al fuel gives higher fluxes than 5 w/o UO₂ which in turn out-performs 8 w/o UO₂.

From Figure 5 we can see that progressively smaller cores provide flux levels that increase rapidly in a non-linear fashion. However, the process of decreasing the core size is limited by the critical mass of the assembly (approximately 3-4 kg of $^{235}\text{U}$ for a core at operating conditions with a full load of fission product poisons) and the requirement for a useful operating period. In this respect cores with 8 w/o UO₂ show the most potential for configurations smaller than 14-fuelled sites because of the larger amount of $^{235}\text{U}$ per unit volume.

It is interesting to note that, at the 10 MW power level, the peak thermal flux in the D₂O reflector (2.1 * $10^{14}$ n·cm⁻²·s⁻¹ with U₂Si-Al fuel) is becoming competitive with much more powerful and expensive reactors, for instance NRU, with peak thermal flux of 3 x $10^{14}$ n·cm⁻²·s⁻¹ at 120 MW power (6).

Figure 8 shows a comparison of the radial variation of the thermal flux, from the core centre, for a number of highly specialized research reactors (2). The MAPLE research reactor performance compares
favourably. The slightly faster radial decrease for MAPLE research reactor results from the use of a 1.4 m diameter D₂O tank for the study. The decrease would have been less for a larger reflector tank.

Figure 5: Peak Thermal Flux in the D₂O Reflector as a Function of the Mass of ²³⁵U In-Core

Figure 6 presents peak outer element ratings because this parameter is usually specified as the limiting operational criterion. The highest values occur on the outer elements of the 18-element reactivity shim assemblies in the inner core. Detailed burnup calculations show that, at the beginning of life, the outer element ratings are 10-14% higher than those of the inner elements. This difference gradually decreases and reverses as fuel burnup progresses beyond 50% IFAC. Under most circumstances 50% IFAC is close to the limit of exit burnup so that the peak outer element rating is the factor that will limit reactor power. For this study limiting values of 50 kW/m (similar to CANDU fuel) for U₀₂ fuel and 85 kW/m for U₃Si-Al fuel were adopted. These values have been indicated by horizontal lines in Figure 6. We can see that the U₀₂ limit is reached when the total core power is about 6 MW. By increasing the core size to 24 fuelled sites it is possible to raise the core power to 8.5 MW. However, there is a flux advantage of 20% to operating the 14 site core at 6 MW over the 24-site core at 8.5 MW. The latter however has a considerably longer core life (3 times the former for 5 w/o U₀₂) and consequently higher exit burnup on the fuel. For a 14-site configuration U₃Si-Al fuel is operating well below the limiting value specified for this study.

8. SUMMARY

Finally, a study of the properties of the radioactive isotopes of interest to medicine, agriculture and industry shows that many have resonance integrals which are much larger than their thermal absorption cross sections. Hence, higher total activation and higher specific activities can be produced by a neutron spectrum which is slightly hardened. As Figure 7 shows, irradiation sites located in the beryllium reflector modules of a 14-site core, for instance, can have a neutron spectrum which has a resonance component that is almost 50% of the thermal flux level. Hence activation can be accomplished by both thermal and resonance neutrons.
FIGURE 7: A PLOT OF PEAK THERMAL FLUX AND THE RATIO OF THERMAL FLUX TO RESONANCE FLUX ACROSS THE LONG AXIS OF THE CORE ASSEMBLY FOR A 14-SITE U₃Si-Al CORE OPERATING AT 10 MW

FIGURE 8: A COMPARISON OF RADIAL PEAK THERMAL FLUX DISTRIBUTIONS FOR VARIOUS RESEARCH REACTORS (NORMALIZED TO 10 MW)

REFERENCES


ASSESSMENT OF BEAM TUBE PERFORMANCE FOR THE MAPLE RESEARCH REACTOR

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ABSTRACT

The MAPLE research reactor is a versatile new research facility that can be adapted to meet the requirements of a variety of reactor applications. A particular group of reactor applications involve the use of beams of radiation extracted from the reactor core via tubes that penetrate through the biological shield and terminate in the reflector surrounding the fuelled core. An assessment of the neutron and gamma radiation fields entering beam tubes that are located radially or tangentially with respect to the core is given.

INTRODUCTION

A major utilization of research reactors involves beams of radiation extracted from the reactor core to an experimental area outside of the reactor shield. For example, beams of radiation are used in neutron scattering studies for solid-state physics, in neutron radiography, in prompt gamma-ray neutron activation analysis and in nuclear physics investigations. Each application has different requirements for neutron and gamma radiation fields.

The radiation beams are extracted from the core of a research reactor via beam tubes that start in the reflector material surrounding the core and penetrate through the biological shield. The composition of radiation fields in a beam depends on many factors, such as the orientation, tangential or radial, of the tube with respect to the core and the type and amount of scattering medium between the tube and the core. For example, tubes that point radially towards the fuelled core region have a larger component of gamma radiation than tubes that are tangential to the fuelled core.

The first generation of multipurpose research reactors were equipped with radial beam tubes and used D_2O, Be, C or H_2O as reflectors. Although the neutron beam performance of these tubes was adequate at the time, a number of these reactors have been upgraded or are approaching upgrade to achieve better performance. More recently, specialized reactors have been equipped with tangential beam tubes and D_2O reflectors for neutron beam research. These specialized reactors provide high neutron flux intensities (greater than 10^{14} n/cm^2/s) for neutron beam research (especially for neutron scattering applications).

It is anticipated that users of a MAPLE research reactor will have different beam tube requirements, depending upon specific applications. It is desirable to have an understanding of the performance characteristics of representative beam tubes to respond to the user requirements. For instance, many neutron scattering studies for solid-state physics have required high thermal neutron content. However, contemporary research in solid-state physics use beams of "cold" neutrons with mean temperatures less than 20 K. Many applications of neutron radiography also require high thermal neutron fluxes, however, epithermal neutron fluxes are needed to radiograph objects that have significant amounts of thermal neutron absorbing species such as boron.

Atomic Energy of Canada Limited (AECL) has developed a new multipurpose research reactor called the MAPLE (Multipurpose Applied Physics Lattice Experimental) research reactor. This research reactor is being designed to satisfy the neutron flux requirements of contemporary research reactor applications. In addition, the MAPLE research reactor is being designed to provide neutron flux intensities in its beam tubes that are comparable to dedicated medium flux neutron beam reactors such as ORPHEE (3x10^{14} n/cm^2/s at 14 MW).

To evaluate the neutron beam performance of the MAPLE research reactors, a study was conducted to estimate the neutron spectra and neutron flux distributions of several beam tube geometries and with different reflector configurations. Computer simulations were performed with a three dimensional Monte Carlo code, MCNP [1]. The computer model was based on a MAPLE research reactor core configuration that consisted of nineteen fuelled lattice sites and ten beryllium filled sites. A reference reactor power level of 10 MW was assumed.

NEUTRON BEAM REQUIREMENTS FOR TYPICAL APPLICATIONS

Before presenting and discussing the results of the computer simulations it is appropriate to examine the neutron beam requirements for some typical beam tube applications.

Neutron Radiography

Most neutron radiography applications use thermal neutrons because the applications involve the detection of elements with large thermal-neutron cross-sections such as hydrogen and boron. Examples of the use of neutron radiography can be found in reference [2].

In general the desirable features of a thermal neutron beam for radiography are:

a) high thermal neutron intensity at the entrance to the collimator. This is most easily accomplished by locating the collimator entrance at the beam tube nose and by making the collimator aperture diameter as large possible.

b) low gamma intensity. A desirable ratio of the thermal neutron flux to gamma ray dose is at least 10^3 n/cm^2/mR.
This technique is useful for trace detection of gamma-rays that are emitted immediately are detected. Following a neutron absorption event in the sample, the sample is placed in the path of a beam of neutrons. The neutron flux incident on the sample.

Neutron Scattering

Neutron scattering experiments require high intensity beams of either thermal or sub-thermal neutrons. Sub-thermal (cold or ultra-cold) neutrons are produced by placing a cold source in front of the beam tube nose or inside the beam tube and as close to the nose as possible. The cold source is basically a moderating medium (e.g., hydrogen, deuterium or methane) maintained at very low temperatures (e.g., 4 K for liquid deuterium). Thermal neutrons entering the cold source are brought to equilibrium with the moderating medium and emerge as sub-thermal neutrons.

High epithermal or fast neutron intensities are undesirable because they appear as background noise at the detectors in neutron scattering experiments. For cold sources, the fast neutrons produce a high heat load in the moderating material and contribute to irradiation damage of the container. Minimizing gamma radiation in the beam tubes reduces the shielding requirements at the sample location. Reducing the shielding requirements in turn reduces the neutron source to object distance, and thus increases the flux incident on the sample.

Neutron Capture Therapy

The technique of boron neutron capture therapy for treating brain tumors is an example of recent biomedical applications of research reactors. The brain tumor is selectively loaded with a 10B-enriched compound and the brain is irradiated in a low energy neutron beam. A study performed at the University of Tokyo [3] concluded that neutrons with energies between 10 eV and .5 eV were optimally suited for this treatment, although thermal neutrons are also suitable. Fast neutrons were found to penetrate too far into the brain and thus were not suitable. Because the patient must be placed with his head in the path of the neutron beam, it is essential to minimize the gamma-ray fields in the beam tube.

Neutron Capture Therapy

The MAPLE research reactor consists of an open-tank-type reactor assembly located within a pool of light water as shown in Figure 1. The reactor assembly consists of three parts: a lower support structure/inlet plenum, the heavy water reflector vessel and core region, and the chimney. Light water coolant enters the inlet plenum, flows upward through the core region and exits from the chimney via outlet nozzles.

FIGURE 1: MAPLE RESEARCH REACTOR STRUCTURE (ELEVATION)

A horizontal view of a typical MAPLE research reactor heavy water vessel and core region is shown in Figure 2. The central core region consists of twenty-nine grid positions arranged in a hexagonal array. This region is light-water-cooled and moderated. Surrounding the core region are fourteen vertical flow tubes. These flow tubes can be fuelled and are light water cooled. The fourteen vertical flow tubes penetrate through the heavy water vessel. Outside of the ring of fourteen flow tubes is a heavy water reflector region. Beam tubes penetrate the heavy water vessel horizontally into this heavy water reflector region.
The heavy water vessel is a cylindrical, annular, zirconium alloy tank. The inner wall of the vessel is in the shape of an elongated hexagon with fluted sides that are welded to the upper and lower tube sheets. The central cavity bounded by this fluted wall is the central core region.

Further details about the MAPLE research reactors can be found in reference [4], which presents a general description of the MAPLE research reactor. Reference [5] gives a description of the MAPLE-X research reactor under construction at Chalk River Nuclear Laboratories.

Two fuel options, UO₂ and U₃Si-Al, are available for the MAPLE research reactor. The UO₂ fuel is proposed for use in MAPLE research reactors up to operating powers of about 6 MW. The U₃Si-Al fuel is suggested for higher power operation. Comparative reactor physics information on these two fuel choices are given in an accompanying paper [6].

The computer simulations for the beam tube studies, presented in this work, are based upon the U₃Si-Al fuel.

**U₃Si-Al Fuel Description**

Two types of fuel assemblies are required for the MAPLE research reactor, 36-element driver assemblies and 18-element shim assemblies [5]. The 36-element assemblies are installed in the hexagonal flow tubes, while the 18-element assemblies are installed in cylindrical flow tubes in the locations of the control and shutdown absorber rods and optionally in the fourteen vertical flow tubes surrounding the central core. The two types of fuel assemblies have similar central Zircaloy support shafts. The 36-element assemblies have the elements arranged on a twelve millimeter pitch. Those of the 18-element assemblies are arranged in two concentric rings of six and twelve elements. Details are given in Table 1 obtained from reference [5].

**TABLE 1: U₃Si-AL FUEL FOR THE MAPLE RESEARCH REACTOR**

<table>
<thead>
<tr>
<th>Fuel Element</th>
<th>U₃Si-39 wt% Al</th>
<th>U-235 in U</th>
</tr>
</thead>
<tbody>
<tr>
<td>Enrichment</td>
<td>61 wt%</td>
<td>20 wt%</td>
</tr>
<tr>
<td>Density</td>
<td>5.43 g/cm³</td>
<td></td>
</tr>
<tr>
<td>Diameter</td>
<td>6.35 mm</td>
<td></td>
</tr>
<tr>
<td>Length</td>
<td>600 mm</td>
<td></td>
</tr>
<tr>
<td>Mass of LEU</td>
<td>60.14 g</td>
<td></td>
</tr>
<tr>
<td>Mass of U-235</td>
<td>12.03 g</td>
<td></td>
</tr>
<tr>
<td>Cladding</td>
<td>Al (co-extruded)</td>
<td></td>
</tr>
<tr>
<td>Thickness</td>
<td>0.76 mm</td>
<td></td>
</tr>
<tr>
<td>Fins</td>
<td>8</td>
<td></td>
</tr>
<tr>
<td>Height</td>
<td>1.02 mm</td>
<td></td>
</tr>
<tr>
<td>Width</td>
<td>0.76 mm</td>
<td></td>
</tr>
<tr>
<td>Diameter over Cladding</td>
<td>7.88 mm</td>
<td></td>
</tr>
<tr>
<td>Diameter over Fins</td>
<td>9.91 mm</td>
<td></td>
</tr>
</tbody>
</table>

**18 Element Fuel Assembly**

- **Shape**: Cylindrical
- **Flow Tube Diameter**
  - Inner: 61.1 mm
  - Outer: 62.8 mm
- **Mass of LEU**: 1,083 g
- **Mass of U-235**: 216.5 g

**36 Element Fuel Assembly**

- **Shape**: Hexagonal
- **Flow Tube Diameter**
  - (Flat to Flat)
    - Inner: 74.4 mm
    - Outer: 76.6 mm
- **Mass of LEU**: 2,165 g
- **Mass of U-235**: 433.0 g

**COMPUTER MODEL OF THE REACTOR**

The reactor core computer model was based on nineteen fuelled sites, thirteen 36-element driver fuel assemblies and six 18-element shim assemblies. The absorber rods were not included in the model. Also, it was assumed that the fuel was totally fresh, that is, fission products were not included in the core model. The fuelled core region, consisting of driver fuel assemblies, shim fuel assemblies, flow tubes and light water, was represented as a single homogeneous cell in the Monte Carlo calculations.

The remaining ten lattice positions in the core region were filled with beryllium assemblies. The beryllium filled part of the core region was represented by two homogeneous cells in the computer simulations. These cells consisted of a uniform mixture of beryllium, light water and zirconium flow tubes.

The heavy water vessel was assumed to be 1.8 m diameter by 1.2 m tall. Depending on the geometry of the beam tube, the heavy water vessel was divided into different numbers of cells for the computations. Figures 3 and 4 show a typical geometry in the MAPLE research reactor and a version of the simplified geometry employed in the computer simulations, respectively. Figure 3 also shows a possible beam tube arrangement around the core region.
The computer simulations were performed with the neutron-photon Monte Carlo transport code MCNP [1]. The combined neutron-photon transport mode was used to accumulate particle tracks in a "source" file for later use in the beam tube simulations. In the computer simulations for the beam tube geometries, the neutron transport cases were run separately from the photon transport cases.

RESULTS

Figure 3 shows an example of the beam tube arrangement investigated in this study. This figure shows four beam tubes, three tangential and one radial. To keep the computer run times reasonably short, only one beam tube was modelled in each computer case.

Table 2 summarizes the results obtained for a tangential beam tube oriented as tube 1 in figure 3. Calculations were performed with the vertical irradiation hole in front of the beam tube alternately filled with beryllium, graphite, light water and heavy water. Table 2 presents the neutron fluxes in five energy groups where the energy column gives the upper limit of each group. The neutrons with energy up to $0.25\times10^{-7}$ eV are commonly referred to as thermal neutrons. The neutrons with energies between $0.25\times10^{-7}$ eV and $4\times10^{-6}$ eV are referred to as epi-thermal neutrons. The next energy group from $4\times10^{-6}$ eV to $9\times10^{-3}$ MeV is known as the resonance region because many elements have sharply increased absorption cross-sections in this energy interval. The next two energy groups are known as the intermediate and fast neutron groups, corresponding to neutrons with high velocities. Most irradiation damage is caused by neutrons in the intermediate and fast energy groups. From table 2, it is observed that the fast flux at the nose of the beam tube is much smaller when beryllium is used as the reflector material in front of the beam tube. Also with light water as the reflector material in front of the beam tube, the neutron fluxes in the epi-thermal and resonance energy ranges are lower than the corresponding fluxes with other reflector materials.

Each beam tube was modelled with an elliptical cross-section within the heavy water reflector and with a circular cross-section outside of the heavy water vessel. The beam tube was modelled as a zirconium tube filled with air. A zirconium end plate separates the air-filled region from the heavy water. The beam tube was centered vertically with respect to the fuelled core. Only one beam tube was modelled in each computer run.

The variation of neutron fluxes at the nose of a tangential beam tube as a function of the thickness of heavy water between the core and the beam tube can be seen from the results presented in table 3. For this case study a beam tube oriented as tube 2 in figure 3 was placed at various distances from the core.
The effect on the neutron fluxes at the nozzle of a radial beam tube from varying the thickness of heavy water between the beam tube and the core was also studied. The results of the calculations are presented in Table 4.

### TABLE 4: NEUTRON FLUXES IN A RADIAL BEAM TUBE AS A FUNCTION OF THE THICKNESS OF HEAVY WATER BETWEEN THE BEAM TUBE AND THE CORE (10^12 n/cm^2/s)

<table>
<thead>
<tr>
<th>ENERGY(MeV)</th>
<th>HEAVY WATER THICKNESS(cm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>5.5</td>
<td>10.5</td>
</tr>
<tr>
<td>6.25 x 10^-7</td>
<td>170 ± 11.5</td>
</tr>
<tr>
<td>4.0 x 10^-5</td>
<td>9 ± 1.4</td>
</tr>
<tr>
<td>9.0 x 10^-3</td>
<td>4.2 ± 0.4</td>
</tr>
<tr>
<td>8.0 x 10^-5</td>
<td>36 ± 17.0</td>
</tr>
<tr>
<td>1.0 x 10^1</td>
<td>8 ± 17.0</td>
</tr>
</tbody>
</table>

The ratio of the thermal neutron fluxes to the gamma radiation doses for radial and tangential beam tubes was also investigated. The results are presented in Table 5. The calculations were performed for each of the four tubes shown in Figure 3.

### TABLE 5: RATIO OF THERMAL NEUTRON FLUXES TO GAMMA DOSES FOR RADIAL AND TANGENTIAL BEAM TUBES

<table>
<thead>
<tr>
<th>Beam Tube</th>
<th>( \phi_{\text{th}}/\gamma ) (n/cm^2/s)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Radial</td>
<td>8.8 x 10^3</td>
</tr>
<tr>
<td>Tangential tube 1</td>
<td>1.3 x 10^6</td>
</tr>
<tr>
<td>Tangential tube 2</td>
<td>7.2 x 10^6</td>
</tr>
<tr>
<td>Tangential tube 3</td>
<td>9.4 x 10^6</td>
</tr>
</tbody>
</table>

**DISCUSSION**

The results presented here represent a series of calculations performed to select beam tube placements for a typical MAPLE research reactor such as the MAPLE-X research reactor under construction at the Chalk River Nuclear Laboratories. These calculations were performed to identify significant trends rather than to provide detailed investigations of the beam tubes.

The fast neutron fluxes (8.0x10^-10 to 1.0x10^-1 MeV group) for beryllium filling the vertical hole in front of the beam tube are significantly smaller. Also, when the hole is filled with light water, the epi-thermal and resonance energy neutron fluxes (the three energy groups from 6.25x10^-7 to 8.0x10^-1 MeV) are lowered. Overall, the placement of different reflector materials directly in front of a tangential beam tube produces only small changes in the neutron fluxes.

The small effect can be attributed to the fact that in each case the neutrons escaping the core pass through about 10 cm of heavy water before encountering the moderating material in the vertical hole. Also, the beam tube is completely surrounded by heavy water, so the moderating ratio of D_2O+Be, D_2O+U, and D_2O+C moderators is dominated by the U.

The results of varying the thickness of heavy water between the tangential beam tube and the core show that the beam tube can be located to significantly reduce the non-thermal neutron fluxes without losing much of the thermal flux intensities. This is of particular importance for experiments where the non-thermal fluxes contribute to the background noise. It is also very important when materials must be placed in the beam tubes and are susceptible to fast neutron irradiation damage.

The ratio of the total neutron flux to the total non-thermal neutron flux is similar to the cadmium ratio and the higher the ratio, the greater is the thermal neutron component of the beam. The results shown in Table 2 to 4 do not give a true cadmium ratio because the thermal neutron cutoff was placed at 0.625 MeV. Nevertheless, the results do indicate the thermal neutron component in the beam. From Table 3 it is seen that the ratio of the total neutron intensity to the non-thermal (greater than 0.625 MeV) intensity varies from about 3:1 to 14:1 when the heavy water layer is varied from 5.5 cm to 20.5 cm. For the radial beam tube, the results from Table 4 give ratios of 4:1 to 7:1 when the heavy water thickness is varied from 10 to 25 cm. As expected the ratio is much lower for radial beam tubes placed at the same distance from the core.

The difference in the performance of radial and tangential beam tubes can be seen by examining Tables 3, 4 and 5. For applications where non-thermal neutron fluxes are undesirable it is evident that tangential tubes are superior to radial tubes for applications where gamma irradiation is not desired.

**CONCLUSIONS**

The results of the computer simulations have provided some insights into the influence of beam tube orientation and reflector thickness on the neutronic performance of the beam tube. More detailed simulations would be required to choose appropriate beam tube placements for a specific set of applications. Also, the influence that the beam tubes have on each other need to be investigated.

The MAPLE-X research reactor will be equipped with several tangential and radial beam tubes. Measurements of the radiation fields in those beam tubes will be very useful for verifying these computer simulations.

**REFERENCES**


ABSTRACT
A new facility based on the MAPLE research reactor concept is being planned for the Chalk River Nuclear Laboratories. The reactor will be used primarily for the production of short-lived radioisotopes and will also serve as a prototype for a MAPLE research reactor.

INTRODUCTION
Atomic Energy of Canada Limited (AECL) has developed a new reactor concept called MAPLE (Multipurpose Applied Physics Lattice Experimental) which is intended to fulfill International requirements for a multipurpose research reactor. It is planned to replace the NRX reactor with a MAPLE type, designated MAPLE-X, to be used primarily for the production of short-lived radioisotopes at the Chalk River Nuclear Laboratories (CRNL). This will enable the NRU reactor to pursue an optimum research schedule without isotope production constraints and, in addition, will provide a prototype MAPLE research reactor. The licensing for the reactor is at an early stage and the licensing process activities, including the safety aspects, are summarized in this paper.

SAFETY PHILOSOPHY
The International Atomic Energy Agency (IAEA) Code of Practice [1] was followed during the preparation of the MAPLE-X safety review. The IAEA approach is similar to that of Canada in that the safety philosophy and principles are agreed to by the licensee and the regulatory authority and it is left mainly to the licensee to ensure that adequate procedures are followed. Also, because reference 1 has evolved through international cooperation, it was considered to be particularly appropriate as the MAPLE concept is being developed for the world market.

Good design practice is fundamental to reactor safety and will incorporate 'defense-in-depth' philosophy through the use of multiple barriers against fission product release; through standard safety design features such as the use of redundancy, diversity, separation of control and protective systems, and fail-safe components; and through the provision of means for thorough testing of all components and circuits. Recognized codes and standards will be used for the design and fabrication of components and systems and the CRNL Quality Assurance program will be applied to all aspects of the project.

The features which contribute to the safety of the MAPLE-X reactor are briefly summarized below.

1. The reactor core will be located within a water-filled pool which provides a large heat sink for fuel cooling and also provides shielding of the fuel. The MAPLE reactor assembly essentially acts like an upright bottle in a sink (draining the sink does not cause the bottle to empty). A failure of either the pool or the primary cooling system will not allow the core to be uncovered.

2. The core decay heat will be removed by natural circulation, either through heat exchangers or through the pool, in the event of loss of flow in the primary cooling system.

3. Where possible the safety of the reactor will rely on inherent passive safety features. Two of these are the negative reactivity effects of both coolant voidage and fuel temperature increase.

4. The control system will be designed to achieve a high level of reliability, thus ensuring a low probability of faults which could require protective system action.

5. Reactor safety systems will be provided to ensure the safe shutdown of the reactor and heat removal from the core and to limit the consequences of a range of postulated initiating events which could lead to accident conditions.

6. The reactor will be designed and operated in a manner which avoids unnecessary exposure to radiation and to keep unavoidable exposures as low as reasonably achievable, social and economic considerations being taken into account, while respecting individual dose limits.

LICENSING PROCESS

Review and Approval Process
Atomic Energy of Canada Limited (AECL) has the prime responsibility for the safety of its own nuclear facilities. A safety review and approval committee, the Nuclear Safety Advisory Committee (NSAC), with representatives from across AECL as well as from the Atomic Energy Control Board acts as the regulatory body. Furthermore, the Chalk River Nuclear Laboratories operate under license by the AECB. In 1980 this arrangement was formalized by the issuance of a site license and the subsequent appointment of a project officer to inspect the nuclear facilities and ensure compliance with the terms of the site license. When a facility is reviewed and approved by the NSAC, the AECB is requested to amend the site license to include the new facility. Upon amendment of the site license, the owner is authorized to operate the facility according to a set of "Principles and General Rules".

To assist in the licensing procedure, the NSAC has issued guidelines for the preparation of safety
The review and approval process includes up to five iterative steps of presentation, study, and discussion involving the applicant, the committee, and various consulting groups. The stages are: a letter of notification; concept review and location approval; construction approval; licensing, startup and commissioning; and submission of an updated Final Safety Analysis Report (FSAR). The major stages are the Concept Safety Assessment Report (CSAR) requesting approval in principle of the concepts involved and the location of the facility, including limited site work up to the point of pouring concrete; the Preliminary Safety Analysis Report (PSAR) requesting construction approval; and a Final Safety Analysis Report (FSAR) requesting approval to start up, commission, and operate the facility. The guidelines outline briefly the information that the NSAC expects to be contained in each particular submission. The committee recognizes that, for a large or complex project, not all the required information may be available when the applicant wishes to commence construction. For this situation the committee requires that all available information be included and a plan and schedule be provided for the remaining work.

The main feature of this review and approval process is that the NSAC encourages the early opening of communications with the applicant. The intent is to identify any points of contention which can then be resolved as early as possible in the project.

**Code Classification**

The reactor structures, systems, and components required for the protection of health and safety are those necessary for assuring safe reactor shutdown and cooling and also those necessary for preventing an uncontrolled release of radioactivity to the environment. The designation of appropriate code classifications ensures that the safety functions of the various structures, systems, and components will be maintained. For MAPLE-X, code classification is based on the Canadian Standards Association, CAN3-N285.0-M81 [2], which requires that pressurized systems and components be classified according to the radiological and safety consequences of hypothetical failures of these systems or components, the consequences of which would not be exceeded by any accident considered credible.

Although CSA N285.0 refers specifically to nuclear power plants, it has been adopted by the AECB for other nuclear facilities including MAPLE-X. The rationale is that design and manufacture may require a high level of confidence in the integrity and reliability of components where monitoring and maintenance must be limited.

Dose limits for research reactors have not been established as they have been for CANDU nuclear power plants, but the basic philosophy is that the risk to the public from the operation of a research reactor should be substantially less than the risk from operation of a nuclear power plant.

The initial approach in code classification is to postulate a single failure in a system and determine the radiological consequences based on conservative assumptions for the actual releases, atmospheric dispersion, and dose conversion factors. If failure of a system results in an off-site dose of 0.5 rem or more whole body, then the system or appropriate components should be classified as Class 1, 2, or 3. If failure results in an off-site dose of less than 5 mrem, then a non-nuclear classification may be applied. CSA N285.0 also places limits on the allowable doses to operating personnel. Upon completion of development work and design, and during preparation of the preliminary safety analysis report the accident analysis is re-examined to confirm the code classification selected in the initial assessment. The initial proposal to the AECB, by the above procedure, requested a code classification of Class 6 (non-nuclear) for the major reactor components.

This process results in an assigned classification which identifies the design, fabrication, installation, and operation requirements and ensures compliance with codes, standards, and provincial statutes.

**GENERAL DESCRIPTION**

MAPLE-X will be constructed in a new structural steel, metal clad building adjacent to the existing NRX building and will operate at up to 20 MWh.

The basic MAPLE concept consists of an open-tank-type reactor assembly within a light water pool. The reactor assembly consists of three parts: the inlet plenum, the calandria assembly and the chimney (Figure 1). The light water primary coolant enters the inlet plenum, flows upward through the calandria assembly and exits from the chimney via two outlet nozzles. The calandria assembly is a cylindrical zirconium-alloy vessel penetrated by vertical flow tubes and horizontal beam tubes.

**Inlet Plenum**

The inlet plenum is a cylindrical stainless steel vessel 2.0 m diameter by 0.8 m high. The plenum serves as a coolant header with two 0.3 m diameter nozzles on the lower part of the plenum providing connections to the primary cooling system.

**Calandria**

The calandria vessel is an annular cylindrical, heavy water filled, zirconium alloy vessel 2.0 m in diameter by 1.2 m in height. The inner boundary is a thin walled plate formed in the shape of an elongated hexagon with corrugated sides penetrating through the calandria and welded to the upper and lower tube-sheets, separating the inner core from the rest of the calandria vessel. This defines a light water region for the inner core (Figure 2) which can accommodate twenty-nine hexagonal or cylindrical fuel assemblies. Surrounding the inner core and within the heavy water filled calandria are fourteen outer fuel sites which are also cooled by light water from the inlet plenum. As shown on Figure 2, additional isotope and experimental irradiation sites are located outside of the chimney and within the calandria and are cooled by natural circulation of pool water.
Outlet Chimney

The chimney assembly consists of a hollow elongated hexagonal structure approximately 0.7 m across the flats and 2.4 m high.

Primary Cooling System

The purpose of the Primary Cooling System (PCS) is to remove the fission heat from the core and to provide purification and chemistry control of the water. A part of the PCS flow bypasses the core and returns to the PCS via the chimney. The equipment includes four pumps, two heat exchangers, a purification system and interconnecting piping and is located in a shielded room adjacent to the service pool.

The PCS, shown schematically in Figure 3, circulates demineralized water into the inlet plenum. The water flows vertically upward through the central core and outer fuelled sites, exits from the fuel channels into the chimney, and rises vertically to pump suction nozzles, one on each side of the chimney. At this point the core coolant meets a lesser flow, resulting from the core bypass flow, coming down the chimney from the pool. Approximately 10% of the total primary coolant that has passed through the heat exchangers is directed to the bottom of the pool instead of to the inlet plenum. This water cools the bottom of the pool and slowly rises around the outside of the reactor structure. Water is drawn into the top of the chimney to balance the effect of the core bypass which was directed to the pool bottom. Thus the pump suction, along with the chimney downflow, prevents the core flow which contains activation products from rising to the top of the pool where it could cause a radiation hazard.

Part of the bypass flow is directed through the purification system to remove impurities and to control water chemistry.
With the pumps shut down decay heat will be removed by thermosyphoning through the heat exchangers or through flap valves on the PCS inlet line (Figure 3). The secondary side of the heat exchangers is supplied by the existing NRX low pressure water system.

**Process Water System**

The process water system is required to supply cooling water to the heat exchangers of the primary cooling system and moderator/reflector system of flows required during normal operation. River water is pumped to the existing low head tank by three electrically driven pumps. A steam turbine driven pump is available for emergency use. The water then flows by gravity from the low head tank (capacity of $3.6 \times 10^6$ L) to strainers, a chlorinator and then to the MAPLE-X heat exchangers.

A second water line runs from the low head tank to the emergency pool make-up system.

**Emergency Pool Make-Up System**

The Emergency Pool Make-Up System (EPMS) ensures that there is an adequate heat sink available for decay heat removal following a loss of the primary cooling system inventory.

The EPMS is supplied by gravity through a line from the process water system low head tank. Two normally closed injection valves (V1-V4) are connected in series on each of two branches of the line (Figure 4). The branches combine downstream of the valves and the line penetrates the pool liner and connects to the primary coolant inlet pipe close to the plenum.

The system is actuated by triplicated level signals at an elevation just above the top of the chimney. The injection valves are closed by a level signal at just below the position where the primary cooling outlet pipes penetrate the pool liner.

The fire water system, which is supplied by diesel powered pumps in the event of a Class 4 power failure, can be connected into the EPMS by installing a spool piece (Figure 4).

The EPMS includes a sump pump and an emergency sump pump. The recovered water from the sump can be returned to the injection line into the primary cooling inlet line if required.

**SAFETY ASSESSMENT**

**General**

A requirement for both the forced or natural circulation mode of operation (and the transition between the two) is that the coolant exiting the core be prevented from migrating to the pool surface because of its high N-16 activity (or other activation or fission products). The flow pattern in the chimney and pool are governed mainly by the inertial force due to the jet of water from the core, the buoyancy force due to the density difference between the hot water in the chimney and the cooler water in the pool, the core bypass flow returning to the top of the chimney, and the suction flow from the chimney via the PCS outlet piping.

A scale model is being used to examine the effects of the various parameters.

**Normal Operation**

The radiological hazard during normal operation is mainly due to activation products, particularly N-16, which is formed when the oxygen component of water captures a neutron $[\text{O}+\text{n} \rightarrow \text{N}]$. N-16 decays with a half life of 7.4 seconds, emitting energetic gamma rays (energy 6-7 MeV). The chimney downflow will prevent significant amounts of activation products from rising to the pool surface. The PCS, which will contain the activation or fission products, will be shielded by the concrete pump room walls and by the pool water.

**Anticipated Operational Occurrences**

Loss of Class 4 Electrical Power is expected to occur at a frequency of about two times per year [3]. In this event the PCS pumps will stop and the reactor protective system will be actuated on low flow in the PCS, on high temperature, or on loss of power to the shutoff rods pressurizing motors. Decay heat removal will take place by thermosyphoning through the PCS heat exchangers.
The process water pumps will also stop but coolant will flow by gravity from the low head tank to the heat exchangers. The low head tank has sufficient capacity to provide normal flow for about two hours but as the reactor is shut down the flow will be throttled automatically by the PCS outlet temperature to provide a much longer cooling period.

Anticipated malfunctions of other systems and components are also examined. Although loss of Class 4 power is considered to be the most probable operating occurrence, conceivable failures of other systems and components are examined below.

**Accidents**

The postulated accidents are grouped in the following categories to simplify the analysis:

- decrease in heat removal by the primary cooling system
- flow blockage in a channel
- loss of pool water
- decrease in heat removal by the process water system
- reactivity transients
- external events.

Each of the above postulated accidents could be caused by one or more initiating events (piping failure, etc.). At this stage in the licensing, order-of-magnitude estimates are made of the probabilities and radiological consequences of the initiating events. This analysis, based on very conservative assumptions, examines events ranging from those resulting in no radiological consequences to those having more serious consequences. These latter events are thus identified as requiring further analysis using more realistic assumptions.

Two initiating events involving a decrease in heat removal by the primary cooling system are briefly described in the following sections. Also, as an example, order-of-magnitude estimates of the probability and consequences of another initiating event, flow blockage of a single channel, are shown.

**Loss of Primary Cooling Inventory Inside the Pool**

A failure of the inlet plenum or PCS inlet line (assumed probability of $10^{-4}$/a) will cause a control room alarm annunciation and a reactor trip on abnormal flow. The circulating pumps will continue to run and the water which is lost from the PCS to the pool will be made up by pool water entering the chimney. No change will occur in the pool water level. Further analysis is required to determine the effectiveness of the cooling and to demonstrate that stagnation will not occur in the fuel channels.

If the rupture is in an outlet line, the flow continues as normal in the PCS. The pool water level does not change because the water being taken out the pipe rupture returns to the pool through the PCS. However, the buoyant plume leaving the core may not be suppressed as there is suction from only one side of the chimney. If so, the reactor would be tripped by high radiation fields at the pool surface due to the N-16 or by high temperatures at the chimney outlet and possibly in the intact outlet leg.

**Loss of Primary Cooling Inventory Outside the Pool**

A piping failure outside of the pool has an assumed probability of $10^{-4}$/a and can occur on either the suction or discharge side of the pump.

If the rupture is on the pump suction side the flow will be disrupted, control room alarms will annunciate, and the reactor will be tripped on a low flow in the PCS or on a low pool water level condition. The pumps will be shut down by the coincident flow and low pool level conditions. The core bypass line in the PCS will be automatically closed on the low pool level conditions. If the leak rate is greater than the pool make-up rate the water level will decrease to the level of the PCS outlet pipe/liner connection. The decay heat will be removed by thermosyphoning through the heat exchanger in the intact loop or through the pool via the flap valves.

A rupture on the pump discharge side has the potential of quickly draining the pool to the level of the outlet piping connections to the chimney. Control room alarms would annunciate and the reactor would trip on low flow in the PCS, low pool level or high radiation fields above the reactor. The low flow condition coincident with low pool level will shut down the PCS pumps. The low pool level condition will also stop the core bypass flow. The emergency pool make-up system would maintain the pool at the level of the primary cooling system piping/liner penetration and decay heat would be removed by thermosyphoning through the pool.

**Flow Blockage**

Complete or partial obstruction of a flow channel has occurred in at least three other pool type reactors. Gasket material was the cause of two of the incidents and broken glass from a viewing box was the cause of the third. MAPLE-X will also be vulnerable to this type of accident because of the potential for objects being dropped over the core. However, with cooling in the upflow direction this problem should be minimized. If the loss of flow causes fuel failure, an alarm on increasing radiation fields in the PCS piping will annunciate and, if fission products reach the pool surface, a reactor trip on high radiation fields above the pool will be activated. The probability of this accident, complete blockage of a fuel channel, is assumed to be in the range of $10^{-6} - 10^{-7}$/a.

The following assumptions are made for the source term for the analysis of this accident.

- 100% of the fuel in a 36 element bundle (largest fission product inventory per channel) melts
- 100% of the noble gases (3.2 x $10^{15}$ Ci estimated from the bundle power) is released from the fuel
- 25% of the I-131 (initially 2.6 x $10^{14}$ Ci estimated from the bundle power) is released from the fuel

The bypass flow, with its burden of radionuclides, is directed to the pool bottom and then rises around the reactor structure. It is expected that most of the flow will return to the PCS by entering the top of the chimney. However, the work with the scale model and the supporting computer modelling has not reached the stage where an accurate estimate can be made of the in-pool behaviour. If the chimney downflow effect is as expected there will be little or no release of radionuclides. As a limiting case, it is assumed that the radionuclides are not retained and that after about 15 minutes the noble gases are released from the pool surface. The Iodine will strongly favour the water phase [4] and a retention factor of $10^{3}$ is assumed for I-131 in the pool water. The releases to the stack for these assumptions would be:

- noble gases - 2.5 x $10^{5}$ Ci
- I-131 - 6.5 Ci
As noted previously, these releases are estimates based on preliminary information and conservative assumptions. This accident will be re-examined when further research and development work is completed.

For an accidental release into the atmosphere the worst weather conditions occurring 10% of the time or a meteorological dispersion equivalent to Pasquill category F as modified by Bryant [5] is assumed [6]. For individuals at locations close to an elevated location on top of a ridge its effective height is increased by an amount equal to the elevation difference between the stack base and dose point [5]. Additional correction factors of 22 m and 11 m can be applied for the effects of lift due to the high stack exit velocity for Pasquill category A and F, respectively [5].

Based on the above considerations, the diffusion coefficients were assumed to be 1 x 10^-5 s/m^3 on-site for Pasquill category A and 2.6 x 10^-6 s/m^3 off-site for Pasquill category F.

Ground level releases have not been determined as the building ventilation system should maintain a negative pressure relative to the outside atmosphere and the releases are directed to the stack. As the building is expected to have a low leak rate, failure of the ventilation system coincident with a release of radionuclides from the pool should not result in hazardous conditions on-site.

The thyroid dose equivalents were calculated for adults on-site and for off-site members of the public at risk using dose conversion factors recommended in CSA N288.2 [7]. These dose conversion factors are for 1-131 and include the other isotopes of iodine which are assumed to be present in the same ratio as that in equilibrium CANDU fuel.

1 Ci/s/m^3 = 740 rem infant
1 Ci/s/m^3 = 560 rem adult

The release of iodine would result in a dose equivalent to off-site members of the public most at risk of 0.01 rem thyroid and a dose equivalent to on-site adults of 0.04 rem thyroid. The estimated thyroid exposures are based on the inhalation pathway.

Whole body external doses for gamma radiation from a cloud of noble gas fission products can be derived from the relationship [8]:

\[ D = 0.25 \frac{E_1}{\psi} \]

where \( D \) is the gamma dose in rem to an individual, \( E_1 \) is the average energy per disintegration in MeV, and \( \psi \) is the exposure in Ci/s/m^3.

This relationship assumes a semi-infinite cloud at ground level and a correction factor (0.5 for Pasquill category F at the site boundary) can be applied to account for the finite dimensions of the cloud. However, this relationship underestimates the dose at distances close to the stack because the ground level plume concentration is much less than the concentration overhead. The shielding program NUSHLD has been used to model the plume at distances close to the stack [9]. The noble gas releases from the postulated 'low channel blockage accident were compared to the releases in reference 9 and the resulting doses estimated.

The release of noble gases would result in an off-site dose equivalent of 0.03 rem whole body and an on-site dose equivalent of 0.13 rem whole body.

As stated previously, this assessment assumes no retention of radionuclides by the primary cooling system and the results are therefore considered to be very conservative.

SUMMARY

Operation of the MAPLE-X reactor, based on preliminary information, should not present unacceptable risks to operating personnel or to members of the public.

The safety assessment of the various MAPLE-X systems is still in preparation for the next licensing stage which is the presentation of the Preliminary Safety Analysis Report.

ACKNOWLEDGEMENTS

The author would like to acknowledge the contributions of D.J. Axford from CRNL and also of CANDU Operations staff to this report.

REFERENCES

A neutron reflection method is developed for measuring the concentration of the neutron absorbing gadolinium in the tanks of the liquid poison injection shutdown system of a CANDU reactor. The feasibility of the method is demonstrated experimentally, and Monte Carlo simulations are utilized to design and model the performance of a proposed device.

### INTRODUCTION

The liquid poison injection shutdown system, the secondary shutdown system (SDS2), of a CANDU reactor utilizes a gadolinium nitrate, Gd(NO₃)₂·6H₂O, solution to neutronic poison the reactor following a trip. The solution is contained in several (six to eight) tanks which are connected via an open-line to the reactor calandria. The solution is injected, when needed, under pressure into the moderator system. In order for this shutdown system to be effective, the concentration of natural gadolinium in the solution is required to be no less than 4000 mg Gd/kg solution.

The effectiveness of the SDS2 is presently insured by periodic measurement of the gadolinium concentration. Typically, a sample is taken weekly from one of the tanks and analyzed in the laboratory using atomic absorption. This is supplemented by mass spectroscopy analysis, usually twice a year, in order to ensure the adequacy of the isotopic content of the material used. Natural gadolinium, as shown in Table 1, contains about 30 percent of the neutron absorbing isotopes Gd-155 and Gd-157. To avoid the remote possibility of using burned-out gadolinium, by a prior reactor irradiation, isotopic analysis is required.

The periodic analysis procedure, in addition to being manual and subject to administrative error, does not provide a continual indication of the effectiveness of the system. It is possible that one or more tanks can be downgraded in gadolinium solution and remain undetected until a sample is withdrawn from the tank a few weeks later. It is desirable, therefore, to have an on-line monitoring device that can provide continually an estimate of the poison concentration in each tank. Such a system would reduce the manual effort required for testing and would improve the control room indication.

In this paper, we present a new non-intrusive method for on-line monitoring of the neutron poison solution in each of the tanks of the SDS2. The method is based on neutron reflection and utilizes external neutron sources and detectors. The details of this technique are given later. We begin, however, by reviewing some of the methods that can be utilized for gadolinium concentration monitoring.

### MEASUREMENT METHODS

Several methods can be considered for on-line monitoring of the poison concentration in SDS2 tanks. However, in order to be able to choose a suitable method for application, one must define the characteristics of the required device. An on-line monitoring system is desired to be:

1. capable of yielding continual measurement of the solution's neutron absorption ability in each of the SDS2 tanks,
2. non-intrusive to the existing piping system,
3. maintenance free, and
4. inexpensive.

An on-line monitoring system is required to provide continual indication that can be accessed from the control room. The system is also desired to provide directly a measure of the amount of the neutron absorbing material available in each tank. This enables the detection of any changes in the isotopic content of the gadolinium. Since the piping network of the SDS2 is designed as a Nuclear Class I system, the monitoring device is preferred to be non-intrusive, to avoid the costly process of modifying the system to accommodate the measuring equipment. The monitoring system needs also to be easy to install, reliable, reasonably maintenance free and inexpensive. Some of the techniques that have been previously proposed for poison monitoring are discussed below and assessed against the above characteristics.
Mass Spectroscopy

Mass spectroscopy provides an accurate means of measuring the isotopic content of an ionized sample. The method is based on the deflection of ions in an electromagnetic field. This provides a direct estimate of the type and amount of isotopes in the sample. The device is delicate, expensive and requires samples to be withdrawn from each tank.

Atomic Absorption

In this method, a light source, of a frequency corresponding to the energy required to excite a gadolinium atom, is applied to an atom vapor sample of the solution. The gadolinium present absorbs the incident radiation to a degree proportional to the concentration of gadolinium in the sample. The apparatus used in this method is a delicate one and is difficult to apply on-line. Moreover, the method is intrusive and does not directly measure the neutron absorption ability of the solution.

Electrical Conductivity

The conductivity of a gadolinium nitrate solution varies with its concentration. A conductivity meter can, therefore, be used for gadolinium concentration monitoring. This requires the introduction of a probe into the tank or the associated piping. However, the conductivity measurement does not provide a direct indication of the neutron absorption ability of the solution. It requires frequent calibration and is easily affected by impurities and the degree of acidity of the solution.

Thermal Neutron Attenuation

This is a direct method for measuring the absorption ability, and consequently the concentration of the neutron poisoning isotopes. A very strong neutron source would, however, be needed, if the method is to be applied directly to the solution contained in a SBS2 tank. This is due to the large distance neutrons have to travel in a highly absorbing medium. The method can, however, be applied to a small circulating volume of solution drawn from the tank. Since available isotopic neutron sources emit fast neutrons, a thermalization assembly around the source is required. The neutrons emitted can be utilized to measure the concentration of more than one solution sample located around the neutron source. This method is simple, capable of providing continual measurements, and relatively maintenance free. However, an intrusive sampling system is needed.

Neutron Flux Depression

One may also consider using the flux depression caused by inserting a sample containing gadolinium solution into a neutron medium. The flux depression can be measured directly or by using neutron activation analysis of an internal flux monitor. The magnitude of the flux depression provides a direct measure of the solution neutron absorption ability. However, this is an intrusive method that requires the withdrawal of a solution sample from the tank or the use of a circulating sampling system.

Neutron Activation

Gadolinium produces non-destructive radionuclides under neutron irradiation. The activity of these nuclides can be used, at least in principle, to measure the gadolinium concentration. Activated analysis, however, requires a large neutron flux that can be only obtained inside a nuclear reactor, and therefore, cannot be applied on-line.

All the systems discussed above are intrusive in nature, and only the conductivity and neutron attenuation, flux depression methods can be easily applied on-line. The conductivity method does not provide a measure of the neutron absorption ability of the solution, while the neutron attenuation and flux depression methods require a continuous circulating system to be installed on each tank. An alternative method that can overcome the above difficulties is, therefore, needed. A new measurement technique is introduced below.

Neutron Reflection Method

The neutron appears to be the most appropriate probe for monitoring the concentration of poison solutions, since it measures directly the neutron absorption ability of the solution. However, the neutron attenuation, flux-depression or activation techniques, discussed above, require intrusion into the existing piping system. To avoid this problem we propose to utilize neutron scattering for measuring the poison concentration. If an epithermal neutron beam is directed towards a tank, the neutrons will be scattered and slowed-down, some to the thermal energy, by the heavy water contained within the tank. The slowed-down neutrons will then attempt to escape from the tank and can be recorded by a neutron detector located outside the tank. The scattered thermal neutrons will, however, be exposed to absorption by the gadolinium contained in the solution, the degree to which these neutrons can escape from the tank is, therefore, inversely proportional to the concentration of the gadolinium absorbing isotopes in the tank. As will be shown later, the maximum neutron scattered flux is obtained at 180 degrees with the incident beam. Neutron reflection, is, therefore, proposed for gadolinium concentration monitoring.

Figure 1 shows a schematic diagram of the proposed technique. The monitoring system consists mainly of a neutron source and a detector positioned on the side of each tank. The detector is surrounded with a cadmium sleeve, such that only thermal neutrons reflected from the tank are seen by the detector. Radiation shielding surrounds the source and the detector. No shielding is required on the opposite side of the tank, since the reactor wall can be used for this purpose.

The neutron reflection technique, as shown above, is non-intrusive and capable of providing continual measurement of the concentration of the neutron absorbing isotopes in the solution. The simplicity of the technique makes it a relatively maintenance free and inexpensive method for poison monitoring. The technique, therefore, meets all the needed characteristics defined earlier. In the following sections, the feasibility of the technique is demonstrated and a conceptual design is presented.
In order to prove the feasibility of the neutron reflection technique, a set of experiments was carried out. A sheet-metal cylinder of a diameter equivalent to that of a typical SDS2 tank, (236 mm in diameter), was used in the experiments. A neutron beam extracted from an americium-beryllium source, average energy of around 5 MeV, was employed. A BF₃ thermal neutron detector and standard counting electronics were utilized. The detector was surrounded with a cadmium sheet such that only neutrons scattered from the tank were detected. Virgin heavy water and gadolinium nitrate salt were supplied by the Point Lepreau Nuclear Generating Station (PLGS) for use in these experiments. Solutions of different concentrations were prepared and their concentrations were verified by atomic absorption analysis performed at PLGS. The gadolinium used was obtained from a shipment whose isotopic content was previously confirmed by mass spectroscopy.

Results

As a first step, a set of experiments was carried out using light water, which has a slowing down power much larger than that of heavy water. More thermal neutrons would, therefore, be produced inside a light water tank. Consequently, if one could not measure the gadolinium concentration in light water using neutron reflection, it would not be possible to measure it in heavy water. The results of the light water experiments are shown in Figure 2, on a semilog graph. The Figure indicates that the detector response is approximately an exponential function of the gadolinium concentration.

The light water experiments were also used to obtain the optimum detector location. This location is defined as the position at which maximum foreground-to-background count ratio is obtained. The experiments showed that the optimum detector position is located a few centimeters directly beneath the neutron source. The same conclusion was obtained for heavy water.

The experimental results obtained using heavy water solutions are also shown in Figure 2. A nearly exponential relationship exists between the detector count rate and the gadolinium concentration, except at low concentrations. The behaviour of this detector response is explained later, when the physical theory behind the technique is presented.

The experiments presented above demonstrate the feasibility of using neutron reflection for gadolinium concentration monitoring. Although, the detector response tends to saturate at high concentration, the logarithm of the signal provides a near-linear relationship that can be used to decide whether the gadolinium concentration has declined. It can be also noted that the reflected signal count rate is always larger than the background signal. This indicates that a useful signal can always be obtained even when the solution in the tank is fully poisoned. Given the success of the experiments, it was decided to perform some computer simulations in order to obtain some insight into the physics of the problem and to further optimize the performance of the techniques.

MONTE CARLO SIMULATIONS

The Monte Carlo method is the most suitable approach to simulating the neutron reflection technique, because of the irregularity of the boundary conditions involved. A modified version of the COM (2) program was utilized for this problem. COM is a Center-of-Mass Monte Carlo program that was originally developed to simulate neutron scattering of a two-phase flow in a pipe. The program calculates the response of a thermal point detector located outside the pipe and provides a plot of the distribution of thermal neutrons generated inside the pipe.

The COM program was used to study the effect of the neutron source energy on the detector response. A set of hypothetical monoenergetic sources, and that
of a californium-252 neutron source were considered. As it can be seen from Figure 3, the detector response is generally an exponential function of the gadolinium concentration, with some deviation at low concentrations. This deviation becomes more pronounced at lower energies. A theoretical explanation of this behaviour is given in the next section.

The measurement resolution, as indicated by the slope of the logarithm of the detector responses, increases with the source energy, as Figure 3 shows. The detector response, however, decreases in value as the energy increases. One, therefore, must choose an appropriate energy at which reasonable resolution and count rates can be obtained.

Monoenergetic isotopic sources are not readily available. Therefore, polyenergetic sources must be used. The americium-beryllium source used in the experiments reported above emits neutrons of an average energy of about 2 MeV. This results in, as Figure 3 indicates, a low detector count rate per source neutron. A californium-252 neutron source, therefore, considered. This is a spontaneous fission source that emits neutrons of an average energy of about 5 MeV. The californium-252 source appears to provide a good compromise between resolution and count rate per source neutron, as shown in Figure 3.

The Monte Carlo simulations were also used to show the distribution of thermal neutron cloud generated inside the tank. Figures 4 and 5 show the distribution of the thermal neutrons generated in a poison-free tank for a high and low incident neutron energy, respectively. The figures show that the concentration of the cloud near the tank boundary closest to the detector decreases as the incident neutron energy increases. The spread of the cloud is utilized in the following section to model the neutron reflection phenomena.

**THEORETICAL MODEL**

For calibration purposes and for better understanding of the system's behaviour, a simple theoretical model is desired. We present here a model based on the observations obtained from the experimental and Monte Carlo results presented above. The model assumes that the distribution of the thermal neutron cloud generated inside the tank is independent of the gadolinium concentration. This is a reasonable assumption, since gadolinium is not a very effective neutron thermalizer. Gadolinium will, however, absorb the generated thermal neutrons as they diffuse throughout the tank. The amount of neutron absorption is assumed to be governed by the exponential relationship:

\[
M_p = M_0 \exp \left(\frac{-Kp}{I} \right) + V(p),
\]

where \(M_p\) is the detector response for a poison concentration \(p\), \(I\) is a calibration constant, \(K\) is an attenuation parameter that incorporates the microscopic absorption cross section of gadolinium and the geometry of the thermal neutron cloud, and \(V(p)\) is a term that accounts for deviation from the exponential function. The exponential nature of the detector response is depicted in the semi-log graphs of Figures 2 and 3, obtained from experiments and Monte Carlo simulations, respectively.

The deviation from the exponential function observed at low concentrations can be attributed to the fact that the effective area of the thermal neutron cloud contributing to the detector increases as the gadolinium concentration decreases. That is, for high gadolinium concentration only neutrons closest to tank boundary near the detector contribute significantly to the detector counts, while neutrons near the center of the tank have a very low probability of reaching the detector. On the other hand, at low gadolinium concentration thermal neutrons generated near the center of the tank have a higher chance of reaching the detector. In other words, the thermal cloud appears to the detector to be more visible at low gadolinium concentrations. This effect is taken into account in equation 1 by introducing the visibility term \(V\). This term is not only a function of the gadolinium concentration, but it is also a function of the incident neutron energy. This is due to the fact that the concentration of the thermal neutron cloud, and consequently its visibility, change with the incident neutron energy, as Figures 4 and 5 demonstrate.

The experimental and Monte Carlo results indicate that the visibility term and Monte Carlo results indicate that the visibility term can be represented by the equation:

\[
V(p) = (M_0 - M) \exp \left(\frac{-Bp}{I} \right),
\]

where \(M\) is the detector response at zero poison concentration, \(B\) is the visibility coefficient at concentration \(p\).

In order to demonstrate the validity of the above model, let us consider the americium-beryllium heavy water experimental measurements, shown in Figure 2. The slope of the line of the logarithm of the dominant exponential function at high concentration, shown in Figure 6, determines the value of the
attenuation parameter $K$. The intersection of this line with the zero-concentration axis determines the constant $I_0$ of equation 1. The visibility coefficient $\beta$ is determined by the slope of the line of the logarithm of equation (2). Figure 6 shows a good agreement between the model and the experimental measurements. Similar results were obtained for the californium-252 Monte Carlo results. This indicates that equation (1) is a valid representation of the detector response. The theoretical model developed above can, therefore, be used to produce calibration curves for field monitoring devices.

Based on the above experimental and theoretical evidence, one can now propose a preliminary design of a monitoring system. The device consists of a 2 microgram californium-252 neutron source and a BF$_3$ detector enclosed inside a 200 mm thick polyethylene shield. The basic configuration of the apparatus remains as shown in Figure 1. The device should be located as close as physically possible to the tank and positioned so that use can be made of the reactor wall as a radiation shield. Vertical motion of the device can permit also the detection of gadolinium precipitation.

Californium-252 is chosen as the neutron source because, as indicated earlier, it provides a good compromise between resolution and detector count rate. Additional advantages of this source are its low heat generation, high specific activity and low gamma radiation output. Therefore, no cooling of the device is required and a minimum gamma-ray shielding is needed. A 5 mm radius sphere of lead surrounding the source is considered to provide sufficient gamma shielding for the proposed source strength.

The main disadvantage of a californium-252 source is its relatively short half-life (2.6 y). However, to obtain the neutron yield of 4.6 million n/s of a 2 microgram (1.07 mCi) californium-252 source, 2.1 Ci of americium-beryllium or 354 mCi of radium-beryllium would be required. The choice of the californium-252 source, therefore, results in a significant reduction in radioactivity, and consequently in the shielding requirements. A device based on a californium-252 source is, therefore, much more compact in size and much less in weight, as compared to a device that uses any alternative isotopic neutron source. The short-half life problem of the californium-252 source is partially compensated for by the fact that the source is widely commercially available at a reasonably low cost. Therefore, the replacement of the source, expected to be no more than three times during the life-time of the reactor, does not constitute a significant addition to the cost of the device. The effect of the change in the source strength with time can be accommodated for through software, or by providing a dummy reference system to which measured signals can be compared. The latter alternative is possible because of the low cost of the device.

A BF$_3$ thermal neutron detector is proposed in the design for economical reasons. The more efficient, and more expensive, He-3 detector should be used, if it can be afforded. In order to improve the signal-to-noise ratio, a cadmium sleeve should be used to cover the surface that is not directly exposed to...
neutrons reflected from the tank. A standard electronics counting system can be used with this device.

CONCLUSIONS

This paper demonstrated the feasibility of a neutron reflection method for gadolinium concentration monitoring in the tanks of the liquid poison injection shutdown system of a CANUH reactor, and presented a conceptual design of a suitable device. The method is non-intrusive and uses off-the-shelf equipment. The signal obtained from the device provides a direct measure of the neutron absorption ability of the poison solution contained in the tank. The device can also scan the tank to monitor gadolinium precipitation. Further work is being undertaken to construct a prototype system suitable for installation at an operating CANUH reactor.

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REFERENCES


COMPARISON OF COMMISSIONING TEST RESULTS WITH PHYSICS SIMULATIONS IN A CANDU REACTOR

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700 University Avenue, Toronto, Ontario, Canada M5G 1X6

ABSTRACT

This paper presents the comparison of simulated and experimental results of various experiments performed during the Phase B Commissioning of Pickering NGS B Units 5, 6, 7, and 8 and Bruce NGS B Units 5, 6 and 7. During Phase B Commissioning, a reactor is brought to criticality for the first time. Once the criticality is achieved, a number of experiments are performed to confirm the nuclear characteristics of the reactor at low power but before the reactor had operated at significant power. These experiments fall into two distinct categories: (a) static tests, and (b) dynamic tests. The static tests comprise the determination of critical poison concentration at the time of first criticality, calibration of zone controllers, and measuring the reactivity of various reactivity devices. The dynamic tests comprise power rundown systems with both the shutdown systems activated one at a time.

The physics simulations were performed before the commissioning of the reactor using the reactor physics computer codes available at Ontario Hydro. The results have shown that our reactor physics codes are able to simulate overall neutron balance and modelling of the reactivity devices.

INTRODUCTION

In Phase B, the reactor core is unirradiated and at very low power. The moderator contains some poison, boron or gadolinium, to compensate for the absence of fission product poisons in the fuel and excess reactivity due to cold conditions of the fuel, moderator, and coolant.

The reactor is brought to criticality for the first time from its guaranteed shutdown state by removing some of the poison from the moderator. The poison concentration at which the criticality occurs is measured and compared with that calculated previously.

Once the criticality is reached, several experiments are conducted to confirm the nuclear characteristics of the reactor at low power. The reactor control and shutdown systems are checked to verify their behaviour as designed. Comparison between experimental results and pre-simulations ensure that the actual reactor parameters agree with the values used in design and accident analysis. This comparison also serves to further validate the computer codes which are used in the design process and which will be used to follow the subsequent operation of the reactor.

A number of static and dynamic simulations are performed prior to the commissioning of a reactor. The static simulations include:

(a) critical moderator poison concentration;
(b) calibration of liquid zone controllers;
(c) calculation of reactivity worth of adjusters; and
(d) calculation of reactivity worth of shutoff rods.

The dynamic simulations include:

(a) power rundown with shutdown system 1; and
(b) power rundown with shutdown system 2.

In this paper, the experimental results are compared with the simulated ones during the Phase B commissioning of Pickering NGS B Units 5, 6, 7 and 8 and Bruce NGS B Units 5, 6 and 7. The results indicate that the reactor physics codes used in the analyses are able to simulate overall neutron balance and modelling of the reactivity devices.

MODELLING METHODS

Core Models

The core is modelled by laying a grid of mesh planes over the core. Cross sections for each mesh volume are obtained from the lattice cell cross sections for the fuel bundle contained within the cell at an appropriate irradiation or averaged over an irradiation interval. Incremental cross sections are added to represent any devices or structures which may be present in the mesh volume.

Mesh spacings are typically one lattice pitch by one lattice pitch by one bundle length (28.575 by 28.575 by 49.53 cm), or finer. Many CANDU core physics codes allow variables mesh spacings. Figure 1 shows a typical CANDU core model.

Super Cell Calculations

Reactivity devices, and some in-core structures, in a CANDU reactor are located in the moderator between the fuel channels and perpendicular to them. In a core model, the effect of these devices is simulated as the sum of the fuel cross section, calculated with the device absent, plus an irradiation independent "incremental" cross section to represent the device.
The "incremental" cross-sections are calculated as follows: an ultra-fine-mesh, three-dimensional, "super-cell" model is set up as shown in Figure 2. Cylindrical devices are rectangularized. Diffusion theory is assumed in the moderator and other weakly absorbing materials. Strong absorbers, such as control rods and fuel bundles, are modelled by imposing current-to-flux ratio boundary conditions obtained from transport theory or analytical calculations. Reflective boundary conditions are imposed at all outer faces of the super-cell.

Flux distributions are computed in two neutron-energy-groups, or one thermal group with a flat epi-thermal slowing down source. The "incremental" cross-section is the difference of super-cell cross-section with and without the device. The incremental cross sections are assumed to be independent of the irradiation of the fuel.

Lattice and Core Calculations

A CANDU lattice cell consists of a fuel bundle immersed in a heavy water coolant, surrounded by a concentric pressure tube, air gap, calandria tube, and finally by an appropriate amount of heavy water moderator.

CANDU lattice cell codes calculate cell-averaged, two-energy-group neutron cross-sections as a function of fuel irradiation in an infinite array of such cells. Leakage is treated by buckling.

The lattice calculations in this study were done using POWERFUS(1), which is specifically written for natural uranium fuel CANDU-type reactor lattice. It performs cell calculations in one dimensional radial geometry.

The core calculations in this study were performed using OHRFSP (Ontario Hydro Reactor Fuelling Simulation Program)(2). OHRFSP performs core design, core physics, and fuel management calculations for CANDU reactors. It calculates neutron flux and power distributions using a two group, three-dimensional finite difference diffusion method. The equations are solved by an iterative Hilbert successive over-relaxation method with updating the eigenvalue every flux iteration. Power is computed by multiplying the flux by an irradiation dependent, power to flux conversion factor, and normalizing to total reactor power. All the static simulations were done with OHRFSP.

Modal Methods

For transients involving a large number of time steps, finite difference methods are computationally expensive. The spatial flux distribution can be approximated at much less cost using modal methods. In these methods, the spatial flux distribution is expanded in series of spatial expansion functions or "modes". With a suitable choice of modes, flux distributions may be calculated with accuracy close to finite difference methods. The space-time dependent problem is then reduced to one of solving a small number of coupled linear algebraic equations.
The modes are generated by solving the finite difference diffusion equations in a core model appropriate to some nominal state. Effects which cannot be properly represented by modal expansion functions are represented by "local effects" correction.

The dynamic simulations, i.e., power rundown with shutdown systems 1 and 2, were done using the codes SMOKIN/SMOKUP(3), based on modal method.

RESULTS

Approach to Criticality

The reactor was initially in a guaranteed shutdown state. The reactor was made critical by removing gadolinium poison from the moderator by ion exchange. Gadolinium concentration was measured during the approach-to-critical by taking samples from the moderator circuit.

The critical poison concentration was calculated by simulating fresh core state of the system using OHRFSP. The reactivity at several poison concentrations was calculated and the critical poison concentration was determined by interpolation. Boron was used in the calculations, then converted to an equivalent gadolinium concentration. The results for all the reactors are given in Table 1. The simulations and measurements agree within the limits of the expected experimental error. These results show that our physics codes are able to simulate the overall neutron balance sufficiently accurately.

<table>
<thead>
<tr>
<th>Reactor</th>
<th>Poison Concentration (mg Gd/kg D2O)</th>
<th>Measurement</th>
<th>Simulation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pickering NGS B</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Unit 5</td>
<td>2.5±0.1</td>
<td>2.56</td>
<td></td>
</tr>
<tr>
<td>Unit 6</td>
<td>2.6±0.1</td>
<td>2.61</td>
<td></td>
</tr>
<tr>
<td>Unit 7</td>
<td>2.7±0.1</td>
<td>2.72</td>
<td></td>
</tr>
<tr>
<td>Bruce NGS B</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Unit 5</td>
<td>2.6±0.1</td>
<td>2.72</td>
<td></td>
</tr>
<tr>
<td>Unit 6</td>
<td>2.5±0.1</td>
<td>2.46</td>
<td></td>
</tr>
<tr>
<td>Unit 7</td>
<td>2.7±0.1</td>
<td>2.46</td>
<td></td>
</tr>
</tbody>
</table>

Calibration of Zone Controllers

The reactivity of zone controllers was measured as follows: with the reactor critical, poison was removed from the moderator and the average water level in the zone controllers was allowed to rise to about 80 percent of full. Then a precisely measured amount of gadolinium nitrate was added to the moderator and the reactor regulating system was allowed to adjust the zone controller levels until the reactor was critical again. Knowing the reactivity coefficient of gadolinium from the simulation and the amount of poison in the moderator, the change in the average zone controller level with a known reactivity change was determined.

This was repeated in steps of approximately 5 to 10 percent zone level until the average zone level was about 20 percent of full.

The reactivity worth of zone controllers was computed by calculating the eigenvalue at various zone levels in each reactor.

Figure 3 shows a typical comparison of measured and computed reactivity of Pickering NGS B Unit 5 zone controllers. Note that the experimental data does not cover the same range as the computed data (0 to 100 percent). A linear least square fit to the zone controller calibration data for all the reactors was done and the slopes from simulations and measurements are compared in Table 2.

<table>
<thead>
<tr>
<th>Reactor</th>
<th>Slope (mk/Percent)</th>
<th>Measurement</th>
<th>Simulation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pickering NGS B</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Unit 5</td>
<td>0.070</td>
<td>0.063</td>
<td></td>
</tr>
<tr>
<td>Unit 6</td>
<td>0.069</td>
<td>0.063</td>
<td></td>
</tr>
<tr>
<td>Unit 7</td>
<td>0.078</td>
<td>0.069</td>
<td></td>
</tr>
<tr>
<td>Unit 8</td>
<td>0.079</td>
<td>0.069</td>
<td></td>
</tr>
<tr>
<td>Bruce NGS B</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Unit 5</td>
<td>0.076</td>
<td>0.067</td>
<td></td>
</tr>
<tr>
<td>Unit 6</td>
<td>0.081</td>
<td>0.072</td>
<td></td>
</tr>
<tr>
<td>Unit 7</td>
<td>0.079</td>
<td>0.072</td>
<td></td>
</tr>
</tbody>
</table>

It is clear from Table 2 that the simulated slopes are consistently lower than the measured ones. The mean error is 0.008 mk with a standard
deviation of 0.0014. This is about 10 percent. The clear bias in one direction suggests that there is some problem. The problem could be in the modelling of zone controllers in OHRFSP or in the calculation of cross sections for the zone controllers. We intend to resolve this discrepancy whenever the time will permit.

**Adjuster Rods**

The reactivity worth of adjuster rods was measured by removing a single rod or a bank of rods, and allowing the reactor regulating system to adjust the zone controller levels to keep the reactor critical. The worth can then be obtained from the difference in zone levels and the calibration curve obtained previously.

The adjuster rod worth was calculated by determining the k-effective, using OHRFSP, with and without a single adjuster or a bank. The difference in the two reactivities calculated, as above, gives the required worth.

In Pickering NGS B Unit 5, individual adjuster rod worths were compared (see Table 3), while in Unit 6, only adjuster banks (see Table 4). In Pickering NGS B Units 7 and 8, the individual rods as well as banks, as shown in Table 5, were compared.

In Bruce NGS B Unit 6, the worth of individual rods and banks were measured and computed. At the time of commissioning, the adjusters were changed from stainless steel to cobalt. Ideally, the design of cobalt adjusters should be such that their neutron properties should match that of stainless steel. However, the cobalt adjusters were found to have more reactivity and produced a different flux/power distribution necessitating some post-simulations. Therefore, in Table 6, we have compared only three individual adjuster rod worths plus all the banks.

In Bruce NGS B Units 5 and 7, the worth of only adjuster rod number 20 (AA20) and all the adjuster rods withdrawn together were computed. The results are shown in Table 6.

In Table 7, the root mean square (rms) errors and the mean and the standard deviation of errors are listed. These are calculated for both individual adjuster reactivity worth and the adjuster banks. The largest mean square error for individual rod worth is 0.06 mk which is about 7 percent of average rod worth. The maximum root mean square error in the adjuster bank worth is 0.21 mk. This is about 6 percent of the average adjuster bank worth.

These results clearly indicate that the agreement between measurements and the simulations is very good. The errors are within the experimental uncertainties.

**TABLE 1**

<table>
<thead>
<tr>
<th>Rod Number</th>
<th>Experiment (mk)</th>
<th>Simulation (mk)</th>
</tr>
</thead>
<tbody>
<tr>
<td>AA1</td>
<td>0.44 ± 0.06</td>
<td>0.41</td>
</tr>
<tr>
<td>AA2</td>
<td>0.46 ± 0.04</td>
<td>0.47</td>
</tr>
<tr>
<td>AA3</td>
<td>0.40 ± 0.03</td>
<td>0.42</td>
</tr>
<tr>
<td>AA4</td>
<td>0.85 ± 0.04</td>
<td>0.82</td>
</tr>
<tr>
<td>AA5</td>
<td>0.78 ± 0.04</td>
<td>0.79</td>
</tr>
<tr>
<td>AA6</td>
<td>0.86 ± 0.04</td>
<td>0.85</td>
</tr>
<tr>
<td>AA7</td>
<td>1.13 ± 0.06</td>
<td>1.13</td>
</tr>
<tr>
<td>AA8</td>
<td>1.12 ± 0.06</td>
<td>1.12</td>
</tr>
<tr>
<td>AA9</td>
<td>1.22 ± 0.06</td>
<td>1.22</td>
</tr>
<tr>
<td>AA10</td>
<td>0.86 ± 0.04</td>
<td>0.95</td>
</tr>
<tr>
<td>AA11</td>
<td>0.88 ± 0.04</td>
<td>0.87</td>
</tr>
<tr>
<td>AA12</td>
<td>0.90 ± 0.04</td>
<td>0.94</td>
</tr>
<tr>
<td>AA13</td>
<td>1.11 ± 0.06</td>
<td>1.11</td>
</tr>
<tr>
<td>AA14</td>
<td>1.13 ± 0.06</td>
<td>1.13</td>
</tr>
<tr>
<td>AA15</td>
<td>1.17 ± 0.06</td>
<td>1.17</td>
</tr>
<tr>
<td>AA16</td>
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</tr>
<tr>
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</tr>
<tr>
<td>Bank B</td>
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<tr>
<td>Banks A and B</td>
<td>4.75 ± 0.12</td>
<td>4.77</td>
</tr>
<tr>
<td>Banks A, B and C</td>
<td>7.83 ± 0.17</td>
<td>7.89</td>
</tr>
<tr>
<td>Banks A, B, C and D</td>
<td>12.00 ± 0.20</td>
<td>12.06</td>
</tr>
<tr>
<td>All Adjusters</td>
<td>19.70 ± 0.20</td>
<td>19.63</td>
</tr>
</tbody>
</table>

* These errors reflect the uncertainty in the measured averaged zone controller level changes and in the reactivity coefficients used to convert average level (%) to milli k.

( ) Rod is in a symmetric position to a simulated rod.

- Not simulated.

**TABLE 4**

<table>
<thead>
<tr>
<th>Bank Number</th>
<th>Experiment (mk)</th>
<th>Simulation (mk)</th>
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<tbody>
<tr>
<td>Bank A</td>
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</tr>
<tr>
<td>Bank B</td>
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</tr>
<tr>
<td>Bank C</td>
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<td>Bank D</td>
<td>1.21</td>
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<tr>
<td>Bank E</td>
<td>7.18</td>
<td>6.90</td>
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TABLE 5

<table>
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<td></td>
<td></td>
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</tr>
<tr>
<td>4</td>
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<tr>
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<tr>
<td>16</td>
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<tr>
<td>6</td>
<td>0.48</td>
<td>0.48</td>
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<tr>
<td>B</td>
<td>2.02</td>
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<td>1.57</td>
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<tr>
<td>5</td>
<td>1.50</td>
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<tr>
<td>C</td>
<td>3.89</td>
<td>3.66</td>
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<td>3.74</td>
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<td>12</td>
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<tr>
<td>10</td>
<td>0.79</td>
<td>0.66</td>
</tr>
<tr>
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<td>1.01</td>
<td>1.04</td>
</tr>
<tr>
<td>15</td>
<td>0.87</td>
<td>0.92</td>
</tr>
<tr>
<td>D</td>
<td>3.39</td>
<td>3.41</td>
</tr>
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<td>E</td>
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</tr>
<tr>
<td></td>
<td></td>
<td>6.80</td>
</tr>
</tbody>
</table>

Shutoff Rods

The reactivity worths of each of the 18 shutoff rods in Pickering NGS B were individually measured against zone controller level as for adjuster rods. The measured and simulated reactivities for shutoff rods in Pickering NGS Units 5 and 6 are compared in Figures 4 and 5. Ideally, all points should lie on the solid line. Any scatter is a measure of deviations. For Units 5, 6, 7 and 8, the measurement and simulation agreed within a root mean square error of 0.14, 0.09, 0.10, and 0.17 mk, respectively. This amounts to about 5, 3, 4 and 4 percent error of average rod worth in Units 5, 6, 7 and 6 respectively.

In Bruce NGS B, as mentioned before, not all measurements were resimulated. In Unit 6, shutoff rods SA13 and SA21 were resimulated. In Unit 5 and 7, a worth for SA20 was computed. The results are shown in Table 8. The root mean square error for Units 5, 6 and 7 is 0.17 mk. This corresponds to about 7 percent of average rod worth.

TABLE 6

<table>
<thead>
<tr>
<th>Rod Number</th>
<th>Experiment Value</th>
<th>Simulation Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>AA04</td>
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<td>0.77</td>
</tr>
<tr>
<td>AA06</td>
<td>0.90</td>
<td>0.92</td>
</tr>
<tr>
<td>AA20</td>
<td>0.78</td>
<td>0.80</td>
</tr>
<tr>
<td>AA22</td>
<td>0.84</td>
<td>0.92</td>
</tr>
<tr>
<td>A11</td>
<td>19.39</td>
<td>19.48</td>
</tr>
</tbody>
</table>

(1) Rod is in a symmetric position to a simulated rod.

These results indicate that the modelling and methods used in our reactor physics codes are quite adequate.

Flux Shape Measurements

The radial flux profile for various configurations was measured using a moveable core fission chamber. The detector was placed in an empty horizontal (in Pickering NGS B) or vertical (in Bruce NGS B) flux detector tube. The detector was moved to various positions along the tube and the signal recorded.

The corresponding configuration was simulated with OHRFSP. Figure 6 shows typical results from Pickering NGS B Unit 5. The maxima in the measured curve correspond to the local flux at the midpoint between fuel channels. This fine structure in the flux distribution is not simulated by OHRFSP. Neglecting this fine structure, the agreement between simulation and measurement was better than 7 percent root mean square errors for all cases.
TABLE 7
Errors in the Adjuster Rods Reactivity Comparison

<table>
<thead>
<tr>
<th></th>
<th>Root Mean Average Error</th>
<th>Standard Deviation</th>
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<td>(mk)</td>
</tr>
<tr>
<td></td>
<td>Single Rank</td>
<td>Single Rank</td>
</tr>
<tr>
<td>Pickering NGS B</td>
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<td>.09</td>
</tr>
<tr>
<td>Pickering NGS B</td>
<td>-.008</td>
<td>.004</td>
</tr>
<tr>
<td>Pickering NGS B</td>
<td>.036</td>
<td>.101</td>
</tr>
<tr>
<td>Unit 5</td>
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<td>.21</td>
</tr>
<tr>
<td>Unit 6</td>
<td>.06</td>
<td>.004</td>
</tr>
<tr>
<td>Unit 7 &amp; 8</td>
<td>.090</td>
<td>.026</td>
</tr>
<tr>
<td>Bruce NGS B</td>
<td>.004</td>
<td>.036</td>
</tr>
<tr>
<td>Units 5, 6</td>
<td>.18*</td>
<td>.06*</td>
</tr>
<tr>
<td>and 7</td>
<td>.040</td>
<td>.180*</td>
</tr>
</tbody>
</table>

*from Unit 6 data only

Power Rundown

CANDU reactors, except Pickering NGS A, have two independent shutdown systems. The shutdown system 1 (SDS1) shuts down the reactor by dropping shutoff rods into the core and the shutdown system 2 (SDS2) by injecting a gadolinium nitrate solution into the moderator.

The flux transient was monitored after each system was fired with the reactor critical at low power. The flux signal, both from out-of-core ion chambers and from in-core flux detectors, was recorded.

This transient was simulated using the modal kinetic methods described earlier.

The purpose of this test was to confirm that the effectiveness of the shutdown systems, as simulated in the accident analyses used in licensing the station, was conservative with respect to actual system performance. Since several pessimistic assumptions were made for these simulations of shutdown system performance, it is only necessary to verify that the observed power rundown with SDS1 is faster than that simulated with pessimistic assumptions. While the
measured power rundown with SDS2 will be shown to be faster than the observed power rundown with SDS1.

Figure 7 shows a typical observed and simulated power rundown for the SDS1 in the Pickering NGS in Unit 5. These results confirm that the actual system indeed performs better than was simulated.

Similar results were obtained in other reactors for both SDS1 and SDS2.

![Graph showing simulated vs measured flux](image1)

**FIGURE 6: PICKERING, UNIT 5**
**SIMULATED VS MEASURED FLUX**

![Graph showing response of in-core fission detector](image2)

**FIGURE 7: PICKERING, UNIT 5**
**RESPONSE OF IN-CORE FISSION DETECTOR**

**CONCLUSION**

Comparisons between simulation and experiment for flux and power distribution, the reactivity worth of various devices and the power rundown with shutdown systems 1 and 2 are quite satisfactory. We do have some problem in the simulation of zone controllers which we intend to rectify. On the whole, the models and methods used in our reactor physics codes have been found satisfactory by a wide variety of measurements.

**ACKNOWLEDGEMENT**

The authors would like to thank the staff of Ontario Hydro's Central Nuclear Services Department and the staff of the Pickering and Bruce Nuclear Generating Station for their assistance in providing the experimental data.

**REFERENCES**

ABSTRACT

The literature addressing potential industrial applications of combined adsorption/irradiation processing is reviewed. The major advantage of this technique is the reduction of the radiation dose required to achieve a particular effect. Previously investigated applications include wastewater treatment, decomposition of chlorinated phenols in aqueous solution, regeneration of activated carbon adsorbents, and acid gas emission control. Applications meriting future investigation include drinking water treatment, treatment of leachate from chemical waste disposal sites, removal of sulphur compounds from hydrocarbon process streams, and chemical synthesis.

INTRODUCTION

Gamma radiation and high energy electrons can induce desirable chemical and biological changes in exposed materials (1). However, the cost of irradiation has been a barrier to wider implementation of industrial radiation processing. In many instances the cost of irradiation is high because the concentration of the target species is low (e.g., chemical pollutants and pathogens in municipal wastewaters) resulting in a major portion of the deposited energy being diverted into nonproductive channels. The radiation energy would be more efficiently used in effecting the desired conversion if the concentrations of the target species were higher (2). In some situations, the desired species can be retained on an adsorbent column. Irradiation of the column can produce the desired conversion of the species concentrated on the column at higher efficiency while the bulk fluid receives a low average dose.

An improved efficiency of a combined adsorption/irradiation process relative to irradiation alone may be due to one or more of the following effects: 1) efficient use of energy deposited in the adsorbent for radiolysis of the adsorbed species, 2) increased use of reactive species produced by radiolysis of the bulk fluid because the adsorbed species is now present in higher concentration 3) increased efficiency of radiolytically initiated reactions of the adsorbed species due to the proximity of other similar species on the surface of the adsorbent and 4) higher effective dose delivered to adsorbed species because their residence time in the radiation zone is longer. Energy deposited in an insulating material by the passage of high energy photons and electrons is efficiently transferred to its surface and leads to the decomposition of adsorbed chemical species. However, the energy deposited in conducting materials is lost as heat. This point is demonstrated clearly in a study by Zhabeo a et al. (3) of the radiolysis of methanol and cyclohexane adsorbed on a number of different solids. Their results show that energy deposited in materials with large band gaps (SiO2, silica-alumina, Al2O3 and KF) resulted in decomposition of the adsorbed species but energy deposited in materials with smaller band gaps (Pt, Pd and activated carbon) was effectively lost. In spite of these observations, most of the adsorption/irradiation processes tested to date use activated carbon as an adsorbent, probably because of its large capacity for adsorption of organic compounds from aqueous media. In these applications the reactive species produced in the radiolysis of water and in the direct radiolysis of the adsorbed species are likely to be the major contributors to the observed radiolytic decomposition.

WASTEWATER TREATMENT

A pilot-scale study of the combined use of activated carbon and 60Co irradiation for treatment of industrial and municipal wastewater was performed by Hay (4). Reductions of over 90% in chemical oxygen demand and bacterial kills in excess of 95% were obtained in several wastewaters at average doses to the bulk water of 50 krad (0.5 kGy). The retention time for organic compounds in this system was stated to be 20 times greater than the time taken for a complete change of the water in the bed. Thus the average direct dose to the organics was claimed to be 1.0 Mrad (10 kGy). Since oxygen enhances the efficiency of the process, a special aeration method was used to attain oxygen concentrations of 11.5 mg/L in the wastewater to maximise process efficiency. The cleaned water was suitable for discharge to navigable waters or recycle to a poultry processing plant used in the study. Hay also determined that there was no loss of weight or volume of the activated carbon adsorbent in three months of continuous use and that the adsorption capacity of the activated carbon was unchanged after 20 cycles of saturation with organics alternating with its radiolytic regeneration in a flow of clean, oxygenated water.

Similar results were obtained by Shubin et al. (5) in the treatment of wastewater from a livestock farm. These workers observed bacterial kills greater than 99.99% in this more highly infected wastewater at radiation doses similar to those of Hay (4). Pilanev and Shubin (6) comment that the dissolved oxygen contained in these wastewaters (8 mg/L) is insufficient to satisfy its chemical oxygen demand (COD) (800 mg/L). The adsorption/irradiation process reduced the COD to 40 mg/L. The radiation chemical yield of COD reduction calculated from these results is equivalent to the addition of 600 molecules of oxygen per 100eV of absorbed energy based on the average dose to the bulk water flow. These high efficiencies are indicative of catalytic or chain reaction mechanisms.

Though a fair degree of understanding of the underlying chemistry of radiolysis in aqueous solution has been obtained in recent years (1), the
basic chemistry of the application of adsorption/irradiation processing to the treatment of municipal wastewater is not understood at present. Clearly, a 1:1 stoichiometric ratio of dissolved oxygen to COD reduction is not required for this application and the efficiency of the process is much higher than would be expected for the direct irradiation of the bulk fluid. Further work is required to obtain a detailed understanding of the chemical reaction mechanism for this process. Additional work is also necessary to determine whether any increase in bacterial inactivation efficiency is obtained.

DECOMPOSITION OF CHLORINATED PHENOLS IN AQUEOUS MEDIA

The radiolytic decomposition of p-chlorophenol dissolved in water and adsorbed on activated carbon in an aqueous environment has been investigated by Wolf et al. (7). They observed that the irradiation of aqueous solutions of p-chlorophenol resulted in dechlorination and production of carbon dioxide and a brown substance similar to humic acid. The decline in chloride ion yield observed in initially air-saturated water above 2 Mrad (20 kGy) dose was attributed to oxygen depletion since the chloride ion yield was constant at 1.4 molecules/100eV in the presence of a continuous oxygen purge. Other work suggests that the presence of oxygen in aqueous solution may reduce dechlorination yields (1). Wolf et al. (7) observed chloride ion and carbon dioxide formation on the irradiation of p-chlorophenol solutions containing activated carbon under oxygen purge; no humic acid or other polymeric materials were observed. The characteristics of the activated carbons used and the p-chlorophenol decomposition yields determined in this study are shown in Table 1. On the basis of the decomposition yields the authors (7) suggest that the radiolytic decomposition of p-chlorophenol is less efficient on activated carbons with a higher proportion of acidic surface groups. However, the surface coverages of p-chlorophenol on the Premnitz R23, R21 and Novit SX carbon samples used for these experiments were 33%, 29% and 3.5%, respectively. Thus an alternative explanation of the observed differences in the decomposition yields would be that a chain reaction which decomposes p-chlorophenol occurs at high surface coverages. There are several possible explanations for the differences between the yields of chloride ion formation and p-chlorophenol decomposition. Wolf et al. (7) suggest that irreversible bonding of chloride ions to the Premnitz carbons and formation of molecular chlorine which may be passed out with the purge gas are possible. Another explanation would be that a chlorine-containing polymer is formed by a chain mechanism operative at high surface coverages. Further work is required to distinguish between these explanations.

Termanath (8) has studied the radiolytic decomposition of pentachlorophenol (Penta) in aqueous solution and in the adsorbed state on activated carbon. The average yield of chloride ions at the dose required for total decomposition of the Penta in aqueous solution was 3.9 molecules/100eV. This result is quite similar to the chloride ion yields obtained in the p-chlorophenol decomposition study of Wolf et al. (7) but since there are five chlorine atoms per molecule of Penta a five-fold larger dose is required for complete dechlorination of the Penta. No difference in Penta decomposition yields due to the different dissolved oxygen concentrations obtained by initial air saturation, continuous air purge during irradiation and irradiation under 15 atmospheres pressure (1.5 MPa) of oxygen were noted up to 0.5 Mrad (5 kGy) dose. Based on these observations, Termanath concluded that dissolved oxygen had no effect on the radiolytic decomposition of Penta in

<table>
<thead>
<tr>
<th>TABLE 1. ACTIVATED CARBON CHARACTERISTICS AND DECOMPOSITION YIELDS* MEASURED BY WOLF ET AL. (7).</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
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<tr>
<td>------------------------</td>
</tr>
<tr>
<td>BET Surface Area (m²/g)</td>
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<tr>
<td>Adsorption Capacity (mg/g)</td>
</tr>
<tr>
<td>Acidic Surface Groups (mol/g)</td>
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<tr>
<td>Basic Surface Groups (mol/g)</td>
</tr>
<tr>
<td>G(Cl⁻) (molecules/100eV)</td>
</tr>
<tr>
<td>G(p-chlorophenol removal) (molecules/100eV)</td>
</tr>
<tr>
<td>Initial Surface Coverage (% of Monolayer)</td>
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</tbody>
</table>

* Yields at 0.5 Mrad (5 kGy) dose based on energy deposited in water.
aqueous solution. However, this point is not unequivocally made in the absence of data obtained in degassed solutions. Since Termaat made this comparison on the basis of data obtained at doses less than 0.5 Mrad (5 kGy) dose, his observations are not inconsistent with the results of Wolf et al. (7) noted in the previous paragraph. The adsorption capacity of activated carbon (Nuchar UV-G from Vestvaco) used was about 300 mg of Penta per gram of carbon. When activated carbon columns loaded with Penta were irradiated in the absence of liquid water surrounding the absorbent, no radiolytic decomposition was observed. In a 1 : 1.3 : 3 adsorbent to Penta, carbon and water, the yields of Penta destruction were between 0.30 and 0.40 molecules/100eV at doses between 0.3 and 1.0 Mrad (3 and 10 kGy). These yields are in the same range as those observed for aqueous Penta solutions. Further tests showed that the adsorption capacity of the activated carbon was regenerated by irradiation in an aqueous medium. On the basis of these data Termaat attributed the removal of Penta in the presence of activated carbon to solution phase radiolytic decomposition. The results may also be explained by the attack of water radiolytic products on Penta adsorbed on the activated carbon. Higher yields of radiolytic degradation in aqueous media observed in the presence of activated carbon with p-chlorophenol are not observed with Penta. However, an activated carbon bed would still concentrate the Penta in the radiation zone and thereby enhance the efficiency of utilization of the reactive radicals produced in the radiolysis of water over that of a dilute flowing stream of Penta-polluted water.

**ACTIVATED CARBON REGENERATION**

An East German patent by Wissel et al. (9) describes the use of an electron accelerator to regenerate activated carbon saturated with organic compounds and microorganisms. A dose of 100 Mrad (1 MGy) delivered by a 1.5-MeV accelerator operated at 100-mA beam current was used to regenerate a 4-mm thick layer of wet activated carbon (50% H2O) on a metallic conveyor belt moving at 1 m/min. The adsorbed substances are decomposed into simple water-soluble and volatile chemical species. An increase in process efficiency is noted when chemical oxidants (oxygen, ozone, nitrogen oxides and sodium hypochlorite) are added to the activated carbon reducing the dose required to about 50 Mrad (500 kGy). The inventors claim that this process has lower energy consumption, carbon abrasion and toxic chemical releases than standard activated carbon regeneration techniques.

**ACID GAS EMISSION CONTROL**

Two different gas phase irradiation processes (10,11) have been demonstrated for the removal of nitrogen oxides and sulphur dioxide from coal-fired boiler flue gases. Both of these processes appear to be economically competitive with other currently available simultaneous SO2/NOx emission control technologies but they are more expensive than the current SO2-only emission control techniques. A study of the efficiency of adsorbing powdery silica to a flue gas mixture prior to irradiation treatment was performed by Tokunaga et al. (12) in an attempt to enhance the efficiency of the radiation treatment process. These workers observed reductions in the concentrations of NOx and SO2 due to adsorption when powdery silica was added to a flue gas mixture in the absence of radiation. The adsorption of NO is probably preceded by chemical oxidation to NO2 or HNO3 since NO removal is only observed when SO2 is present in the gas mixture and has been adsorbed on the silica surface. The NO2 removal efficiency is enhanced by irradiation of a flue gas mixture containing powdery silica but the SO2 removal is almost independent of radiation dose at high powder feed rates. Feeding the powdery silica prior to irradiation of the mixture gives more efficient NO2/SO2 removal than feeding after irradiation. This flue gas treatment concept may have considerable potential if the quantity of adsorbent required can be reduced substantially.

A patent on the use of an electron accelerator to remove sulphur from coal has been granted to Rny and Feldman (13). The coal is pulverized, slurried with water and subjected to a radiation dose of at least 1.58 Mrad (15.8 kGy). The patent claims that the sulphur present in the coal is converted to gaseous sulphur compounds, elemental sulphur, soluble sulphates and easily separated insoluble sulphates by this process. The separation system required downstream of the irradiation zone are rather complex and may have a substantial impact on the process economies. Only an outline of the process is given in the patent and a fair amount of experimental work would be necessary to evaluate its potential for commercialization.

**OTHER APPLICATIONS**

Several other possible applications for combined adsorption/radiation treatment processes are of interest. Treatment of drinking water by a combined adsorption/radiation process would produce lower levels of potentially carcinogenic trihalomethanes in the water than those produced by the typical chlorination processes (14), though some degree of chlorination following the irradiation stage may still be required to maintain antibacterial action downstream of the treatment facility. This requirement would be similar to that of drinking water treatment plants that use ozonation for primary treatment. Another possible application is in the removal and in situ detoxification of hazardous organic compounds, including polychlorinated biphenyls (PCBs), from dilute wastewaters, such as those leaching from hazardous chemical disposal sites. Adsorption/radiation treatment may also be applicable to the removal of sulphur compounds from hydrocarbon process streams. Finally, many scientific papers describe the yields of various chemicals from the radiolysis of precursors adsorbed on insulating adsorbents (see Cockelbergs et al. (15) for a review of this area). Some of these chemicals may be candidates for industrial synthesis by the adsorption/radiolysis process. We are open to collaboration with industry in the development of these and other applications of radiolysis of adsorbed species.


SESSION C: SAFETY AND THE ENVIRONMENT

Chairman: E.C. Card, W.L. Wardrop & Associates Ltd.

A safety review of the NRU effluent heat recovery project.  
P.R. Ballantyne, AECL CRNL

Chalk River area seismicity and its implications for low-level radioactive waste disposal facility siting.  
J.S. Devgun, AECL CRNL

A systematic approach to the analysis of waste management systems.  
A. Buchnea, MacLaren Plansearch Inc.

Ra 226 and Pb 210 concentration ratios in terrestrial and wetland plants on inactive and abandoned uranium mill tailings in Canada.  
M. Kalin and M.P. Smith, Boojum Research Ltd.

A highly selective method for removing natural radioactivity from drinking water.  
M. Gascoyne, AECL WNRE
A SAFETY REVIEW OF THE NRU EFFLUENT HEAT RECOVERY PROJECT

P.R. Ballantyne

Nuclear Safety and Technology Branch
Atomic Energy of Canada Limited
Chalk River Nuclear Laboratories
Chalk River, Ontario, KOJ 1J0

ABSTRACT

The NRU effluent heat recovery project (fHRP) diverts heated effluent water from the NRU process effluent weir and distributes the water for various heating applications in both the inner and active area at Chalk River Nuclear Laboratories (CRNL). The dominant hazard of the system operation is from leakage of tritiated heavy water from the reactor heavy water system into the light water system and the subsequent contamination of the steam system. Protective features include continuous leakage monitoring and automatic isolation of the recovery system. Modelling of the worst case accident, predicts a dose equivalent from tritium in steam humidification of about 26 mrem (260 μSv). The operation of the heat recovery project does not present an unacceptable risk to CRNL personnel.

INTRODUCTION

A CRNL proposal to recover waste heat from the process cooling water of the NRU research reactor process effluent weir was accepted by the Ministry of Energy, Mines and Resources for partial funding under a retrofit program. The project nominees also received an award (Discovery Award 1984) for proposing a new concept for using low grade heat from the NRU reactor effluent. The heat recovery scheme involves diverting up to 471 kg/s (6280 Ipm) of heated effluent water from the NRU process effluent weir and distributing it for various heating applications in both the inner and active areas at CRNL.

This paper concentrates on the safety and potential operational hazards of the system, outlines the features that will reduce the hazards, and examines the pathway for radioactive exposures that are modelled for both accident and chronic releases to the system. A brief description of the reactor cooling and the heat recovery systems precede these safety discussions.

NRU REACTOR DESIGN

The NRU reactor is a 135 MW (thermal), heavy water moderated and cooled research reactor located at Chalk River. NRU is used for fundamental research, radioisotope production and engineering experiments for power reactor development, including Loss of Coolant Accidents tests. The reactor is cooled by circulation of heavy water from the bottom of eight heat exchangers into a bottom header by eight pumps as shown in Figure 1. The header distributes the water via a tube plate to the rod cups and forces water up through the fuel rods. Outflow is mainly through the orifice section above the fuel, with a small flow where the fuel rods sit in the cups. The heated heavy water then flows into eight volutes at the top of the reactor and flows down through the eight heat exchangers and returns to the pump intakes. Process water is pumped from the Ottawa River, through the secondary side of the eight heat exchangers and returned to the river via the process weir.
pressure on the process cooling lines, ensuring that the heat exchanger shells remain full and ensures that the heat exchangers cannot be siphon drained. The weir also provides a means for thoroughly mixing the outlet water before temperature measurements are made for power calculation. Typical winter time process cooling water flows are 60 000 L/min while summer time flows peak at 90 000 L/min.

HEAT RECOVERY SYSTEM

The heat recovery project involves diverting 28 200 L/min (6280 gpm) of the effluent water at approximately 30°C from the process effluent weir and distributing that water for various heating applications in both the inner and active areas at CRNL. The potential energy recovery is about 25 MW. The project is estimated to cost about $2.5 M (1985). With an estimated 70 percent availability, the payback period is 6.4 years with the system complete as it stands now (no south loop) or reduced to 4.7 years if the south loop is completed. Partial winter 1986 operation indicates that normal availability may be closer to 75%; but can vary widely due to scheduled reactor shutdowns and unscheduled reactor trips. The five distribution circuits and their respective flows are shown in Table 1. The heated water will

![Figure 3: Weir Take-off Piping Details.](image)

take-off is into an isolated room inside the building. Leak detectability is provided. The main drawback to this design is that should a leak occur, that leakage will be into the building. Great care was taken to design a multibarrier seal and to demonstrate the grouting effectiveness before actual installation was undertaken.

A manual butterfly valve is installed, as an integral part of the take-off, to provide a means of isolation, should a leak develop in the take-off header piping. A blank flange can be installed on the wet side of the take-off as shown in Figure 3. This was used during completion of construction of the heat recovery system.

All five circuits have isolation valves which are closed automatically when the heat recovery system is tripped or shut down.

SAFETY CONSIDERATIONS

Operational Hazards

The main potential hazard from operating the heat recovery system is from the transfer of radioactivity to other buildings by way of the plumbing. An internal leak in one of the main heat exchangers could allow tritiated heavy water to contaminate the process water. With this water being distributed by the heat recovery system around the plant, three potential pathways for exposure to tritium exist. (The hazards from both fission products and activation products were analyzed in the original safety report [1], but are not considered here, since the tritium dose is the dominant hazard).

The pathway of most concern is coincidental leakage in the service water heat exchanger in the gravity feed circuit with a main heavy water heat exchanger leak because the preheated water is used wherever domestic water is used. Of primary concern was the fact that service water is also used for drinking water. To reduce the possibility of contamination of the service water, a double tube heat exchanger was installed in the powerhouse. This design provides three barriers that require coincidental failure and is a very low probability event. The vented annulus between tubes

### Table 1: Heat Recovery System Distribution

<table>
<thead>
<tr>
<th>CIRCUIT NUMBER</th>
<th>LOCATION</th>
<th>APPLICATION</th>
<th>MAX WINTER FLOW</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>1gpm</td>
</tr>
<tr>
<td>1</td>
<td>NRU Plenum and Fan 7</td>
<td>Space Heating</td>
<td>650</td>
</tr>
<tr>
<td>2</td>
<td>Bldg 440</td>
<td>NRR Process Water</td>
<td>1000</td>
</tr>
<tr>
<td>3</td>
<td>CRNL-North West Loop</td>
<td>Space Heating</td>
<td>1830</td>
</tr>
<tr>
<td>4</td>
<td>CRNL-East, East Loop</td>
<td>Space Heating</td>
<td>1670</td>
</tr>
<tr>
<td>Gravity</td>
<td>Bldg 420 Power House</td>
<td>Boiler Feed</td>
<td>130</td>
</tr>
</tbody>
</table>

*The 30°C water flows through the service water heat exchanger and then drains to the fire pump well.

have both indirect and direct uses. Indirect uses include preheating the main plant service water supply and the inlet air to a number of buildings. Direct uses include main steam plant boiler makeup, fire water makeup and NRR process makeup. The latter two were included in the design so that the likelihood of freezing the water supplies in storage tower, in cold winter temperatures, would be reduced.

The 60 cm diameter weir take-off design was the product of a number of considerations and restrictions. The take-off location must not jeopardize the primary function of the weir as described earlier. It should avoid the possibility that a system pipe failure could drain the weir. This would also preclude the possibility of connecting with existing piping prior to entering the weir. The location of the weir take-off should be at a location that is well known with regard to the location of reinforcing bar and weir structure soundness. The location chosen should not create a major hazard due to water leakage. The elevation chosen is shown in Figure 2, with Figure 3 showing the multiple barrier design used to ensure leak tightness. Since the

![Figure 2: Weir Take-off Piping Details.](image)
will indicate a leak from either side of the double tubes.

A second pathway for transfer of activity is the leakage in any system piping system with coincidental failure of a main heat exchanger tube. The random leakage in the piping would likely result in a low flow detection in that circuit and cause automatic isolation of the leaking piping or visible detection of water. This pathway has two barriers with the coincidental failure of the main heat exchanger considered a low probability event.

The third pathway, which has only one barrier is via the gravity feed line to the powerhouse. Effluent water is used as boiler makeup and would result in tritiated water contaminating the steam. Steam is still used in some areas for humidification of dry winter air. This third pathway has only one safety barrier, consisting of the main heavy water heat exchanger and is the pathway that the remainder of this paper will address.

**Leak Detection and Activity Monitoring**

The majority of the process water that discharges to the effluent weir has been used as secondary cooling for the reactor. Process water makes a single pass through the secondary side of the heat exchangers and is then returned to the reactor via the weir. If a leak should develop in one of these main heat exchangers, transfer of tritiated heavy water to the process water would be possible. The operating pressures in the heat exchangers (Figure 2) show that the leakage could be into or out of the heavy water system, depending on the leakage elevation.

Existing activity monitors, on the effluent lines, are used as the continuous or real time leak detection monitors. These monitors are located, in shielded boxes, next to each discharge pipe and are very sensitive to activity in the piping. These monitors will detect any activity in the piping, but during reactor operation, the isotope N-16 is the principal activity in the heavy water. N-16 is an activation product in the heavy water and is produced in quantity only when the reactor is operating. With a half life for N-16 of 7.5 seconds, activity detection in the heavy water becomes more difficult when the reactor is shut down. Travel time to the monitor also becomes important since levels of activity decay very quickly. This was a major factor in the decision to use existing monitors which are located approximately 15 seconds travel time from the outlet heat exchanger, the more likely leakage zone considering relative system pressures. Based on tests done when these monitors were originally installed, no leakages of the order of 2 cc/min of heavy water into light water will be detected. These monitors have a small radioactive source to keep them off the zero point and will alarm on low level to give protection against head failure. Thus these monitors give alarm indication of both high and low activity levels.

As a backup to these monitors, manual sampling on a once per eight hour shift basis is also done. Reliable detection of tritium in water samples in the active area lab can be made at a level of 0.5 µCi/L. In the last few months levels have been measured at less than 0.1 µCi/L. A detection level of 0.5 µCi/L corresponds to a heavy water leakage rate of 1.5 cc/min with 20 Ci/L tritium content in the heavy water.

A guillotine failure of a single tube in the upper regions of a main heat exchanger is believed to be the most serious, credible accident associated with the operation of the EHRP. If we assume that the continuous monitor fails to detect the leakage, the tritium activity transferred to the steam used for live steam humidification would result in an estimated whole body dose equivalent of 25.8 mrem (0.26 mSv) for an 8 hour exposure.

The leakage rate from the failed tube is estimated to be 9 L/min and continue until a total leakage of 500 L will occur. When 500 L has been removed from the heavy water system (after 55 minutes), the receiver expansion tank (a reactor heavy water system tank) will drop to the point where reactor DP0 level control pumps will gas lock, and the heavy water level in the reactor will drop below the automatic trip level. At this point the reactor trip will cause isolation of the heat recovery system and an end to the further contamination of the steam system. It is interesting to note that a catastrophic failure of all tubes in the heat exchanger would result in an out
leakage of more than 15 000 L/min and result in a reactor trip in about 2 seconds, and a subsequent system isolation long before the contamination front would reach the isolating valves. It takes about 70 seconds for water flowing into the bottom of the weir to reach the distribution pumps.

To make an estimate of the dose due to tritium in steam used for humidification, one must determine the tritium contamination in the steam. A model of the boiler/steam system was used to estimate the tritium concentrations in steam over the eight hour exposure period. A simplified model of the boiler/steam system is shown in Figure 4. Keep in mind that the total

To model the activity in the boilers, the amount of tritium in the boiler holdup water is defined as "A". From Figure 5, the rate of change of that activity can be defined and the differential equation solved for each period under consideration.

Period 1 (t = 0 to 30 minutes)

The rate of change of the activity in the boiler is

\[ \frac{dA}{dt} = 1.82 - 0.043A \text{ Ci/min} \]

At \( t = 0 \), \( A = 0 \) and the activity in the boiler during period 1 is:

\[ A = 42.3 - 23.3e^{-0.043t} \text{ Ci} \] (1)

Period 2 (t = 30 to 55 minutes)

The rate of change of the activity in the boiler is

\[ \frac{dA}{dt} = 1.82 - 0.043A + 0.0195A \text{ Ci/min} \]

At \( t = 30 \) min, \( A = 30.6 \) Ci and the activity in the boiler during period 2 is:

\[ A = 77.4 - 42.5e^{-0.0235t} \text{ Ci} \] (2)

Period 3 (t = 55 to 480 minutes)

The rate of change of the activity in the boiler is

\[ \frac{dA}{dt} = -0.043A + 0.0195A \text{ Ci/min} \]

At \( t = 55 \) min, \( A = 51.4 \) Ci and the activity in the boiler during period 3 is:

\[ A = e^{5.2-0.0235t} \text{ Ci} \] (3)

At \( t = 480 \), \( A \) is essentially zero.

To simplify the calculations of the estimated dose of a person who spends 8 hours working in a building humidified by the steam carrying tritium from the boilers, an average contamination in the steam during each of the three periods will be assumed based on a straight line relationship using the values at the limits of each period. The steam contamination at any
time is \( A_{\text{avg}} \) Ci/24500 kg. Weighting the contamination over the 8 hour period, the average contamination of the steam is \( 1.05 \times 10^{-3} \text{ Ci/m}^3 \). With a steam in air ratio 1 to 60, the tritium in air contamination is calculated to be \( 2.27 \times 10^{-5} \text{ Ci/m}^3 \). The estimated committed dose from an 8 hour exposure using a dose conversion factor from N288.1 [2], a breathing rate of 16 L/min and a factor of 2 for skin absorption would be

\[
\text{Dose} = \text{Intake} \times \text{dose conversion factor} \times 2 \\
= 2.27 \times 10^{-5} \text{ Ci/m}^3 \times 16 \text{ L/min} \times 60 \text{ min/h} \\
8 \text{ h} \times 1 \text{ m}^3 \times 0.74 \times 10^5 \text{ mrem/Cl} \times 2 \\
= 25.8 \text{ mrem}
\]

**Chronic Dose Estimate**

The on-line monitors will detect leakage rates of the order of 2 cc/min while the manual sampling can detect leakage rate reliability below 1.5 cc/min (0.5 \( \mu \text{Ci/L} \) sample). As outlined in the monitoring section, tritium levels of the order of 0.1 \( \mu \text{Ci/L} \) and less are common analysis results. If it is assumed that the worst case non-detectable chronic release rate of 1.5 cc/min exists, one can model the boiler inventory of tritium as for the accident case and make an estimate of the chronic dose due to steam humidification.

The simplified model for the boiler with the maximum chronic tritium leakage is shown in Figure 6.

**FIGURE 6: CHRONIC TRITIUM RELEASE BOILER MODEL.**

The rate of change of the activity in the boiler is:

\[
dA/dt = 0.0003 - 0.043 \text{ Ci/min} \\
\text{A} = 0.01277 \frac{\text{Ci}}{\text{min}}
\]

When \( t \) is large, the tritium inventory in the boiler is:

\[
\text{A} = 0.01277 \text{ Ci}
\]

The concentration in the steam is 0.52 \( \text{Ci/kg} \), which translates to tritium in air contamination of 1.12 \( \times 10^{-8} \text{ Cl/m}^3 \).

The estimated committed dose for an 8 hour exposure would be

\[
\text{Dose} = \text{Intake} \times \text{dose conversion factor} \times 2 \\
= 1.12 \times 10^{-8} \text{ Cl/m}^3 \times 16 \text{ L/min} \times 60 \text{ min/h} \\
8 \text{ h} \times 1 \text{ m}^3 \times 0.74 \times 10^5 \text{ mrem/Cl} \times 2 \\
= 0.013 \text{ mrem}
\]

* 1 mrem = 10 Sv

A 200 working day exposure would result in an estimated committed dose of 2.5 mrem.

**Other Safety Concerns**

The dose due to fission products in the heavy water has been analyzed, based on 1986 samples and is small relative to the tritium hazard [3]. The tritium hazard will reduce when the Tritium Extraction Plant is placed in service and tritium concentrations in NRU heavy water are reduced. The dose due to fission products could increase if the heavy water becomes particularly contaminated due to a fuel failure. If this should occur, then an administrative decision would have to be made to take the heat recovery system off-line.

The long term buildup of activity in the non-active inner area buildings was a concern for some people in regards to activity measurements or effects on experiments. To monitor this, a system of lithium fluoride dosimeters [i.e. TLD's] is being used to measure the relative buildup in a selected steam condensate tank. The TLD's will be checked annually against a TLD monitoring background levels.

Of a more serious concern, and one which is being evaluated during the construction stage, is evaluating the effects of building ventilation shutdowns caused by a system trip. To protect the water heating coils from freezing, the intake fans of each building connected to the system shut down when flow is lost to the coils. The fan will remain down until an increase in powerhouse steam production can make steam available to supply existing heating coils. This down time may likely be 30 minutes and may be up to 90 minutes depending on the steam loading, the fan start sequence and the rate of firing up of boiler at the powerhouse. This interruption in building ventilation supply air could have a detrimental effect on the normal movement of air in that building. This interruption could be particularly serious in buildings containing fume hoods where radioactivity or hazardous chemicals are handled. Face velocities could be reduced below acceptable working standards, or in fact result in reversal of fume hood exhaust.

To ensure that no dangerous situations are created by extended supply fan shutdown, simulated tests will be conducted. The tests will involve closing windows and doors (as would be the case during winter heating periods), shutting down the designated supply fans and measuring the face velocities of fume hoods and active cells to ensure positive flows within recommended standards.

Problems encountered with loss of building supply fans are corrected before space heating is placed in service.

**CONCLUSION**

The NRU Heat Recovery Project weir take-off is sufficiently high in the weir that the main heavy water heat exchangers cannot be siphoned and the weir function is not at risk. The take-off piping is robust and the seal design multilayered such that leakage is unlikely to occur. Adequate effluent piping isolation is possible should a leak occur. A monitoring system will detect any significant leakages of heavy water into the process water while routine samples will detect extra low level leaks. Multiple barriers provide adequate isolation of critical pathways where possible. Simple modelling of boiler/
steam system provides a means of estimating the dose by the most credible pathway. Based on dose estimates, the operation of the NRU Heat Recovery Project should not present an unacceptable risk to plant personnel while, at the same time, providing a large annual saving in CRNL energy costs.

REFERENCES


INTRODUCTION

Siting of a radioactive waste disposal facility requires the consideration of many technical and socio-economic factors. Assessment of seismicity of the area is necessary not only because areas of high seismic activity are considered unfavourable as facility sites but also, seismic effects may need to be considered in the design of the facility.

At Chalk River Nuclear Laboratories (CRNL), a prototype shallow land burial facility (SLB-P1) is planned for the disposal of low- and intermediate-level radioactive wastes. The facility is to be sited within the 37 km² CRNL property. CRNL is located in an area where the frequency of seismic activity is relatively high even though the magnitude (M) of earthquakes has been historically <4 on the Richter scale. Since the long term stability and integrity of a radioactive waste disposal facility are of primary concern, it has been necessary to analyze the Chalk River area seismicity and assess its implications with respect to siting such a facility in the area. These implications are primarily in three categories:

1. site suitability
2. design and engineering of the facility
3. regulatory concerns.

This paper provides the relevant seismic data, analyses and a discussion of the relevant implications. It also identifies the soil related concerns important to the acceptability of CRNL's proposed SLB-P1 site.

BACKGROUND INFORMATION

Facility Description

The concept adopted for the shallow land burial facility at CRNL envisages an open-bottom, closed-top trench with reinforced concrete walls and cap, located above the water table. The trench will be approximately 100 m long x 20 m wide x 8 m deep with a usable depth of at least 6 m. A cross-section of the preliminary design of the facility is shown in Figure 1.

The engineered barriers to radionuclide migration may include the leach-resistant waste forms, waste packaging, use of buffer and backfill materials having high sorption capacities for radionuclides and a minimization of waste/water contact. On the intrusion-resistant concrete cap, a "wick effect barrier" may be placed to engineer the drainage of precipitation infiltrating through the top soil away from the facility.

Site Description

CRNL is located on the south bank of the Ottawa River, 150 km upstream from Ottawa (Figure 2) and lies geologically in the Grenville Province of the Canadian Shield. The Precambrian bedrock of the area is extensively faulted, folded and fractured. Unconsolidated sediments of late and post-glacial age mantle much of the bedrock in the area. The overburden at CRNL generally consists of hooldey, silty, sandy till overlain by fluvioglacial sands (and occasional gravels), aeolian sands and, in the wetland areas, recent organic sediments.

A thick blanket of channel sands that forms the Petawawa-Deep River sand plain in the Chalk River area was deposited during the high stages of the
Ottawa River, when precursors of the Upper Great Lakes drained through the river. Water levels in the river dropped approximately 9,500 years ago and a brief episode of aeolian activity led to the formation of dune ridges and sheet deposits of sand.

The site selected for SLB-P1 is located on a sand dune where depth to water table is about 10 m. Water table monitoring over the past six years has shown a variation of about ± 0.5 m around the nominal level. The groundwater flow at the site is relatively rapid (0.45 m per day) and a large volume of water (about 0.5 million cubic metres per year) from a small seasonal lake, called 233 Lake, that lies to the northeast of the dune, recharges the aquifer beneath the dune. This recharge from the lake plus the direct infiltration of precipitation on the dune flow southwest and the groundwater discharges into a perennial wetland about 450 m from the dune. The water becomes a part of the Maskinonge Lake-Chalk Lake system, eventually reaching Ottawa River at Chalk Bay.

The climate of Chalk River area is temperate humid with mean annual precipitation (measured as rain) amounting to about 730 mm. Porosities of the sands range from 0.38 to 0.42. Horizontal hydraulic conductivities range from $6.3 \times 10^{-5}$ m s$^{-1}$ for fine sands to $1.5 \times 10^{-4}$ m s$^{-1}$ for medium sands. Vertical hydraulic conductivities are lower on average by a factor of two to three. The underlying till has horizontal hydraulic conductivity of about $3.5 \times 10^{-6}$ m s$^{-1}$. More detailed information on the SLB-P1 site is available in reference (1). A stratigraphic cross-section of the dune at the site is shown in Figure 3.

**SEISMICITY ANALYSIS**

**Analysis of Historical Seismicity**

Records of seismicity in Canada are maintained by the Geophysics Division of Geological Survey of Canada (prior to April 1, 1986, Earth Physics Branch of Energy, Mines and Resources Canada). Canada is divided into five seismological regions - Eastern, Central, Western, Northern and St. Elias.

**FIGURE 4: SEISMIC ZONES IN EASTERN REGION (FROM REF. (4))**

In the WQU, the frequency of seismic activity is relatively high and historically a large number of small to moderate and a few large earthquakes have occurred in this zone. Chalk River lies in the southern part of this zone close to the southern boundary of the so-called "Gatineau Triangle" which bounds most of the activity within the WQU. The earthquake epicentres are concentrated in the central portion of the zone in southwestern Quebec, north of the Ottawa River.
Based on the Canadian Earthquake Epicentre File maintained by the Geophysics Division, a composite plot for all the earthquakes since 1661 (to 1985) with $M \geq 3$ is shown in Figure 5 for an 800 km x 800 km square region with Chalk River at the centre. The concentric circles around Chalk River location are at 100 km, 200 km, 300 km and 400 km radii. The triangular zone delineated by dashed line is the so called "Gatineau Triangle". Figure 6 shows a close-up of the area with more pronounced activity, showing all events including the microearthquakes. It should be noted, however, that these plots take no account of the poor epicentral accuracy of the pre-instrumental events. During the first two and a half centuries since 1661, the data are based on the assessments of available accounts of felt reports of the earthquakes and have been dependent on the existence of earliest population settlements in the region. Only after about 1927 were a significant number of earthquakes being located instrumentally.

Most of the earthquakes in the region (see Figure 7) had $M < 4$, a moderate number between 4 and 5 and twenty recorded events had $M \geq 5$. The larger earthquakes in the region have included: 1732 earthquake of magnitude about 6 near Montreal, the 1935 Temiscaming earthquake of magnitude 6.2 at Temiscaming, the 1964 earthquake of magnitude 5.6 near Cornwall and the 1983 earthquake of magnitude 5.6 near Long Lake in northern New York State. The effects of large earthquakes can be felt over large distances and damage at distances of 400 km from the epicentre is conceivable depending on the magnitude of the event and the nature of the bedrock and the soil in the area.

The 1935 Temiscaming event had its epicentre about 150 km from Chalk River and it is clear from Figure 6 that the dense part of seismic activity of the region lies relatively close to Chalk River, even though, the recent earthquakes (since 1970) in this part are typically of low magnitude ($M < 4$) with only a few exceeding magnitude 4. The activity within the 100 km radius of Chalk River is quite limited and is of low magnitude.

Based on the historical seismicity, the per annum recurrence rates of earthquakes are shown in Figure 8. For $M < 4$, the data base for the period 1970-1985 is used because the low magnitude instrumental coverage since 1970 has been available in the region and these data are likely to be more representative. Prior to 1970, not all low magnitude earthquakes have been recorded; especially, in the era of the felt reports, very few such earthquakes were reported and presumably, a large number of such events could have
gone unfelt and unreported in areas with no human settlements. Such considerations necessitate that qualitative judgements be applied to the historical data to seek out the representative sets within the data base. The microearthquakes (M < 3) have occurred at a rate of about 25 per year within the 800 km x 800 km square region and earthquakes with M > 5 have occurred at approximately once every ten years. The recurrence rate for M > 6 is not representative since it is based on only one recorded event since 1661.

From Figure 8, the cumulative per annum recurrence rates of 6.168, 0.605, 0.108 for M > 3, M > 4 and M > 5 respectively, have been used to draw the recurrence plot (for the 800 km x 800 km square region) shown in Figure 10, where an adopted maximum magnitude of 7.0 (the value for the WQU) has been imposed. The recurrence curve is similar to that for the WQU (see reference (4)). The methodology is available in references (3, 4).

The earthquakes that have occurred in the vicinity of Chalk River include the following: swarm activity of magnitude 3.0, 4.1, 4.2 on 1963 October 15 near Rapides Des Joachims (about 30 km from Chalk River), of magnitude 3.0 on 1963 October 17 and swarm activity of magnitude 3.3, 3.9 and 3.8 on 1964 January 8. Two very recent events occurred near Chalk River; one of magnitude 3.1 on 1985 August 24 with its epicentral location 70 km southeast of Chalk River (it was felt in Renfrew, Ont.) and the other of magnitude 3.3 on 1986 January 10 with its epicentral location 25 km southeast of Chalk River (it was felt in Pembroke and Petawawa, Ont.).

Two larger events which have received a good deal of attention and which are also worth mentioning here are the Temiscaming and Cornwall events. These are briefly discussed below.

The Temiscaming event (location 46.78°N, 79.07°W) of magnitude 6.2 occurred on 1935 November 1 near the town of Temiscaming, Quebec. It was felt in Toronto, Ottawa, Cornwall and Montreal. There was some damage to buildings but no casualties. It caused a landslide on a railway embankment 290 km away (5). It was also reported to have made Lac Tee, a small but deep lake, appear milky for several weeks following the earthquake. Recent sonar profiles carried out in 1982, have revealed that gyttja on the steep sub-aqueous slopes had slumped, probably as coalescing mudflows forming hummocky deposits at the lake bottom (6). Disturbed soft-sediments were also found in Lac Kipawa.

The Cornwall event (location 44.97°N, 74.90°W) had a magnitude of 5.6 and occurred on 1944 September 5 near the towns of Cornwall, Ont. and Massena, N.Y. The damage, though widespread, was relatively minor with no casualties and only a few buildings showing extensive damage. One characteristic, however, had been the terrifying sounds that accompanied this event.
Seismotectonics, Geological Features and Seismicity

Earthquakes in eastern Canada are intraplate events as opposed to interplate events along the west coast of Canada, U.S.A. and Mexico. The earthquakes along the St. Lawrence River area may be attributed to movements along the faults that form the contact between the tectonic provinces of Grenville and Appalachia. In the Attica Zone (Fig. 4), the seismic activity is believed to be associated with the Clarendon-Linden fault (3). Major faults in Atlantic Canada are described in reference (7).

The seismic activity in the region of interest around Chalk River is difficult to correlate with any geological structures with any degree of certainty. The bedrock of the area, a complex of Precambrian rocks, is extensively faulted (with numerous small faults) with most of the faults lying in a north-westerly direction parallel to Ottawa River. A main fault runs along the Ottawa River. Most of the activity, however, is concentrated north of the Ottawa River in the southwestern part of Quebec.

The seismotectonics of the WQU is not yet well understood. There have been some attempts in literature to correlate the geological features of the area to seismic activity. The characteristics of the WQU have been discussed by Forsythe (8) and seismotectonics of the northeastern United States and adjacent Canada have been analyzed by Yang and Aggarwal (9). Both papers correlate the gravity anomalies to earthquake locations and it is inferred (9) that gravity gradient suggests the existence of structural inhomogeneities in the upper crust. It has also been hypothesized (8) that the concentration of seismic events in the WQU is related to a Grenville Metasedimentary Belt (MB) that covers this area. Figure 11 depicts the MB and the Paleozoic fault zone along the Ottawa River.

**FIGURE 11: GEOLOGICAL PROVINCES AND STRUCTURAL FEATURES (FROM REF. (3))**

(The superimposed seismic data symbols are same as in Fig. 5)

The seismicity in the "Gatineau Triangle" appears to correlate well with the topographic depression of the area. Most of this area lies below the 300 m elevation above mean sea level (asl) while the surrounding areas to the north, northeast, northwest and southwest lie above the elevation of 300 m (asl). The St. Lawrence Lowlands are to the south of the triangle. The depression and the seismicity cross into northern New York state. It has been suggested that the general correlations of the area's seismicity with the MB and the topographic depression perhaps imply some deeper structural control, the surface expression of which may be the topographic depression (3).

Probabilistic Seismic Risk

Up to the 1980 edition of National Building Code of Canada (NBCC), the earthquake loading requirements for structures were specified in terms of the 1970 seismic zoning map (10). In this map, four seismic zones, 0 to 3, based only on the ground acceleration, represented zones of negligible, minor, moderate and major seismic risk respectively. The boundaries of these zones were defined by peak horizontal ground acceleration (based on a 1% probability of exceedance per annum) contours. According to this map, Chalk River is located in zone 2 i.e. the zone of moderate seismic risk.

The 1985 edition of NBCC, however, has two new seismic zoning maps, one based on the peak horizontal ground acceleration (PHA) and the other on the peak horizontal ground velocity (PHV) based on 10% probability of exceedance in 50 years; see reference (11) for these maps. There are seven zones in each map with zone 0 denoting the negligible risk and zone 6, the highest. There is more detailed and refined zoning in these maps in the middle range than that available in the 1970 map. Details of the methodology and the development of these maps have been compiled by Basham et al. (4) and their engineering applications (and the uncertainties associated with the ground motion parameter values) are discussed by Heidebrecht et al. (12). According to these maps Chalk River lies in zone 2 for PHV and zone 4 for PHA.

It should be noted that while these ground motion parameters are useful in engineering design (peak acceleration is the parameter which is still predominantly used in seismic design), the sustained levels and the duration of the seismic shaking are also important considerations. It should be noted that the structures or facilities (including the radioactive waste disposal facilities) which may be considered "critical structures" by the appropriate regulatory bodies, may require higher seismic standards than those provided in the NBCC.

For Chalk River (for a site located 45.9°N, 77.45°W), the calculated PHA and PHV values are given in Table 1 for several probabilities of exceedance per annum (the data are plotted in Figure 12). These values are for firm ground. For the probability of exceedance of 0.002 per annum (once in 500 years), PHA is 0.167 g and PHV is 0.086 m/s^2. These values are relevant because the hazardous lifetime (defined as period of time over which the radioactive wastes present a potential radiation hazard to humans under conditions of disposal) of the waste going into the SLB-PI will be less than about 500 years.
TABLE I: SEISMIC RISK CALCULATED FOR CRNL AREA

<table>
<thead>
<tr>
<th>Probability of exceedance</th>
<th>0.01</th>
<th>0.005</th>
<th>0.002*</th>
<th>0.001</th>
</tr>
</thead>
<tbody>
<tr>
<td>per annum</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Peak horizontal acceleration ((g))</td>
<td>0.072</td>
<td>0.105</td>
<td>0.167</td>
<td>0.241</td>
</tr>
<tr>
<td>Peak horizontal velocity ((m/s))</td>
<td>0.028</td>
<td>0.047</td>
<td>0.086</td>
<td>0.130</td>
</tr>
</tbody>
</table>

(Source: Geophysics Division, Geological Survey of Canada)

* calculations actually done for a probability of exceedance of 0.002105

In general, selecting the acceptable level of probability of exceedance involves judgement based on the intended lifetime of the structure, possible consequences of potential seismically induced failures, regulatory requirements and other factors. The seismic design of the SLN-PI will take into account the probabilistic seismic risk of the area and other relevant considerations.

For the 800 km x 800 km region defined earlier, the PHA and PHV contours (based on the point calculations, by Geophysics Division of Geological Survey of Canada (13), at a 2° latitude x 2° longitude grid spacing) for the probabilities of exceedance of 0.01, 0.005, 0.002 and 0.001 per annum were drawn and examined. Figures 13 and 14 show these contours, as examples, for a probability of exceedance of 0.002 per annum. Chalk River lies in an area where the risk gradient appears to be relatively steep. These contours also provide some reasonable estimates of the PHA and PHV values that can be expected in different areas within this region for this probability of exceedance.

FIGURE 12: PLOT OF CALCULATED PROBABILISTIC SEISMIC RISK

FIGURE 13: PHA CONTOUR MAP

(For a probability of exceedance of 0.002 per annum; values on contour lines are in \(g\))

IMPLICATIONS FOR LOW-LEVEL WASTE DISPOSAL FACILITY SITING

Site Suitability, Facility Design

The discussion in the previous sections highlights the need to assess and take into account the long-term seismic risk in the Chalk River area for designing and developing any radioactive waste disposal facilities since the time period of concern for such facilities may extend into several hundred years. A large earthquake might possibly damage the engineered structure of the repository and/or create failures that could breach the containment barriers and could thus lead to the migration of radionuclides from the facility. The nature of the waste disposal concept and the type and design of the facility will determine to a large extent the integrity of the facility in an area's known seismic risk. Alternatively, the
River area, soil liquefaction is more important and magnitude 7.6 the next day; more than 9000 requires more attention. It is discussed later in this section in somewhat greater detail.

Seismic risk analysis for a site (as a part of the overall siting studies) provides an important input to the engineering design of the facility.

The seismic design of the structure will take into account risks of direct damage to the facility. For a filled and buried repository, the major seismic concerns are soil related. These can include:

1. Potential liquefaction of soils
2. Concerns on slope stability
3. Possibility of collapse or general subsidence
4. Modification of groundwater flow
5. Effect on topography and surface waters.

Many examples of soil failures are available in literature but the Niigata earthquake of 1964 June 16 in Japan (magnitude 7.3) and the Alaskan earthquake of 1964 March 27 (magnitude 8.4) provide perhaps the most dramatic illustration of such failures and their results. Some relevant analyses of these earthquakes are available in references (14-17). The extensive damage that occurred in the downtown core of Mexico City during the recent earthquake of 1985 September 19 (magnitude 8.1 with epicentral location 350 km west of Mexico City, followed by an aftershock of magnitude 7.6 the next day; more than 9000 casualties) can also be related to soil failures. The central part of the city sits atop an ancient lakebed consisting of soft sediments ranging to a depth of about fifty metres.

Among the soil related concerns for the Chalk River area, soil liquefaction is more important and requires more attention. It is discussed later in this section in somewhat greater detail.

Slope failures have occurred during several earthquakes. As an example, during the Alaskan earthquake, large landslides occurred along the coastline of the Turnagain Heights area of Anchorage.

For disposal concepts such as the SLB, the trenches dug in relatively loose sediments will have shallow sloped walls and the concerns can be alleviated by taking adequate precautions during construction and filling periods. Given the historical seismicity of the area and the short time period of construction and filling, the risk (and the consequences) of trench slope failure are negligible. The sand dune on which the SLB-Pl site is located has also shallow slopes and the long-term slope stability concerns for the site are also minimal.

Subsidence caused by an earthquake can also occur if a thick groundwater aquifer exists beneath the site or if there is mining activity in the area. General consolidation of loose sediments can occur during strong seismic shaking. Lowering of the water table (e.g. removal of large amounts of groundwater) and the changing of a differential in the groundwater level across the site can also possibly lead to subsidence.

The Niigata earthquake mentioned earlier caused a general subsidence of some areas so that parts of Niigata city near the Shinano River were permanently inundated with water. As a result of the extraction of gas from deeper rock strata below the city, there had been a continual slow subsidence for many years prior to the Niigata earthquake.

In the CRNL area, although there is no mining activity, the concerns related to the water table changes and the possible consolidation of sediments during seismic shaking leading to settlement or differential settlement are being assessed.

Earthquakes can also produce fissures and faults in the geological strata which can affect the permeability of the rock and modify the groundwater flow characteristics. This in turn may change the site's ability to act as a natural barrier to radionuclide migration.

The SLB-Pl is being based in sand above the water table. The permeability of till underlying the sand deposits is about two orders of magnitude lower than that of the underlying sands and the till forms the lower boundary of the aquifer for all practical purposes. For SLB-Pl, these concerns are thus quite small. For rock cavity concepts, however, such concerns might be more important.

Seismic activity can also modify topography (for example, by triggering landslides) and can change the surface water regimes. The SLB-Pl site is located on the dune ridge, the northeastern side of which forms
one margin of 233 Lake which is a small and seasonal lake that is empty part of the year. Even though the slope on the lake side is more steep as compared to the southwestern side of the dune where the gentle slope extends up to 450 m from the dune ridge, the concerns of modification of immediate topography and drainage pattern are minimal. There is no surface outlet from the lake and thus no potential undercutting of the banks. There is little runoff on the dune because the high permeability of sands allows the precipitation to infiltrate the sands quickly. The sand dunes in the area have been stable for several thousand years.

Soil liquefaction during strong seismic shaking appears to be the single important concern for the CRNL's surficial deposits, the upper layers of which consist of fine to very fine sand and where depth to water table is about 10 m below the ground surface. When saturated cohesionless soil is subjected to earthquake loading conditions, the tendency to compact is accompanied by a progressive build-up of pore water pressures. The soil may remain stable for some number of cycles and then in the absence of any pore pressure dissipation, suddenly lose all its strength with the accompanying development of pore water pressures equal to the applied confining pressure. The saturated soil turns into a fluid porridge and is no longer able to support structures or objects. This sudden loss of strength and rigidity corresponds to the liquefaction of soil.

Liquefaction of saturated soils have been observed during several strong earthquakes. In both the Niigata and the Alaskan earthquakes, liquefaction of soil led to extensive damage. In the Niigata earthquake, in particular, extensive liquefaction of sand in the low-lying areas of the city led to the settlement of structures, often with severe tilting. With liquefaction developing over large areas, automobiles, structures and other objects gradually sank into the resulting quick sand. In some areas buoyant buried structures floated to the surface; for example, a sewage treatment structure that was buried with its base about 6.5 m below ground, emerged about 3 m above the surface after the earthquake. Other physical evidence of soil liquefaction included the appearance of numerous sand vents in the area.

The epicentre of the earthquake was located about 56 km from the city of Niigata and maximum ground acceleration at Niigata was later estimated to be about 0.16.; from a seismograph located in the basement of an apartment building. The onset of soil liquefaction under the building is presumed to have occurred after about 8 seconds of shaking (14).

Extensive studies following the earthquake (14, 15, 17) have confirmed that the difference in behaviour between the areas with extensive damage and areas of slight or no damage is attributable to major differences in soil characteristics. Extensive most of these areas were underlain by sand to a depth of about 30 m (alluvial deposits range in total up to 60 to 90 m depth below ground surface), the areas showing slight or no damage had considerably denser sands and the water table was much deeper. The water table was close to the surface in the areas showing extensive damage, in places being only a metre or so below the surface.

The consequences of soil liquefaction in Niigata and other earthquakes demonstrate the need for evaluating such concerns. For any radioactive waste disposal facilities located in the Chalk River area, the possibility of a strong earthquake occurring during the long period (several hundred years), over which containment of the radionuclides is of concern, may be low but cannot be ruled out. At the SLB-P1 site, the depth to the water table is about 10 m below ground surface. Since the site will be excavated to about 8 m depth and the facility based in the unsaturated zone above the water table, the risk may be small. However, given the seasonal water table fluctuations, a possibility of water table rise over the long term, the low relative density of wind-deposited sand and, given the seismicity of the Chalk River area, it was decided in a preliminary analysis to investigate these concerns further (18).

The Division of Building Research of the National Research Council of Canada (NRCC), carried out Cone Penetration Testing (CPT) at the SLB-P1 site using piezo cone probes and the cyclic triaxial tests on the sand specimens from the site in the laboratory.

The sand to a depth of about 8.5 m was found to be dry and loose with a relative density between 40 to 50%. The sand below 10.5 m, which is fully saturated, had relative density in the range of 75 to 90%. Between 8.5 m and 10.5 m (the position of water table at the time of CPT in 1984 November), the sand was partially saturated due to capillary rise and the seasonal water table fluctuations. It is also the transition zone between the aeolian and fluvial sands. Some loose layers were detected in the transition zone and there were some concerns that if fully saturated and if no pore pressure dissipation is considered during the build-up, these layers could liquefy for maximum ground acceleration of 0.25; or more.

As the site would be excavated to about 8 m depth, in accordance with the NRCC suggestions the density of these layers will be increased by compaction to ensure resistance to liquefaction under anticipated ground movements. The results of these investigations are also being factored into the design of the facility.

It should be noted that, for different sites, any determination of liquefaction potential and the seismic stabilization approach adopted have to be on a case-by-case basis because soils can vary a great deal from place to place even in a single location. There are also large variations in depth to water table.

Regulatory Concerns

While the seismic qualification requirements in Canada for the siting and design of CANDU nuclear power plants are well established (19-22), the regulatory and licensing requirements related to radioactive waste disposal facility siting are still evolving. Of course, it can be expected to be required to demonstrate within the provisions of the safety requirements, that adequate containment and sufficient impedance to the movement of radionuclides would exist under natural event conditions including the earthquakes.

In Consultative Document C-104, the proposed regulatory policy statement issued recently for public comment (23), the Atomic Energy Control Board (AEBC), outlines the regulatory objectives, requirements and guidelines for the disposal of radioactive wastes. The proposed requirements are based in terms...
of the predicted radiological risk to individuals not exceeding pre-set limits. For deriving such risk estimates, among other factors, the susceptibility of disposal options to natural disruptive events such as seismic and tectonic phenomena also needs to be taken into account.

For the high-level wastes, in another AECB Consultative Document C-72, (24), the siting considerations for underground disposal (in geological formations) include that the region in which the repository is sited should be geologically stable. Some general information on the AECB approach to the storage and disposal of radioactive wastes is available in references (25-28).

The U.S. Nuclear Regulatory Commission, in its licensing regulations (29) for the land disposal of low-level radioactive wastes, requires the evaluation of various factors related to seismic activity. With respect to the siting of low-level waste disposal facilities, the 10 CFR Part 61 states: "Areas must be avoided where tectonic processes such as faulting, folding, seismic activity or vulcanism may occur with such frequency and extent to significantly affect the ability of the disposal site to meet the performance objectives of Subpart C of this part or may preclude defensible modelling and prediction of long-term impacts". In the section on summary of comments, it is explained (in response to the question of relevance of seismic or volcanic hazards) that the requirement provides the Commission a mechanism for site specific evaluation of such factors as recurrence intervals, probabilities, liquefaction potential and ground accelerations to compare against the long-term (500 year) radiological hazard and the disposal requirements of Part 61.

The International Atomic Energy Agency recommendations on underground disposal of radioactive wastes also include the evaluation of seismic risk (30, 31) and seismicity has relevance to scenario analysis in the safety assessment (32).

The NBCC (11) specifies the seismic loading requirements for the structures and gives the analysis methods for their determination. However, the radioactive waste management facilities in Canada are regulated by the AECB and the seismic provisions in this regard may be unique.

Although the seismic risk for an SLB facility for low-level wastes is of much less concern in terms of consequences than for a nuclear power plant, it has to be considered for a longer period of time even though the radiological hazard also decreases (through radioactive decay) significantly with time.

CONCLUSION

For radioactive waste disposal facilities, the long-term stability and integrity concerns necessitate an analysis of the seismic risks. Such risks have been assessed for a proposed shallow land burial facility for low- and intermediate-level wastes which is to be located at CRNL. It should be possible to design an engineered facility that takes into account the relevant seismic concerns.

ACKNOWLEDGEMENT

The author wishes to acknowledge the assistance provided by the staff of Geophysics Division of Geological Survey of Canada.

REFERENCES

(1) BEVUN, J.S., KELLEY, R.W.D., "Site Characterization for a Shallow Land Burial Facility at


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* Reports in TR series are unpublished reports available from Atomic Energy of Canada Limited, Chalk River, Ontario KOJ 1LO.
A SYSTEMATIC APPROACH TO THE ANALYSIS OF WASTE MANAGEMENT SYSTEMS

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ABSTRACT

A systematic analytical approach is valuable in choosing and licensing the optimum waste management option. Using a component model (e.g., CHINTEX, described in this paper), a framework within which to describe the behavior of the waste facility/environment system can be constructed. Within this framework, design and field data requirements can be established. When applied early in a project, the more expensive design and field work can be focussed and the likelihood of overlooking critical impacts reduced. The approach was applied to the choice of decommissioning options for uranium tailings and the analysis of alternatives for the disposal of uranium refinery wastes to demonstrate its value.

INTRODUCTION

Waste management in the nuclear industry represents a diverse and complex discipline. The wastes requiring disposal include large volumes of uranium tailings, uranium refinery wastes, and contaminated soils associated with historic activities involving the handling of radioactive material. There are also reactor wastes, wastes from the industrial, medical and university use of radionuclides, and low level wastes that are by-products of processes that are not related to the nuclear industry. The contaminants associated with these wastes include naturally-occurring radionuclides, man-made radionuclides, and non-radioactive chemical contaminants.

There are numerous sites that are presently inactive and in need of remedial works, there are sites that are presently in operation that require decommissioning in the near future, and there are sites that are in various stages of design and planning. Analysis of these waste management systems is required in order to ensure that the optimum designs are chosen and that the environmental impacts will be acceptable. This analysis is complex because of the diversity of sites, wastes, and designs that are involved. In addition, to adequately analyse the waste-environment interaction, the transport of contaminants through the environment, and the impact of these contaminants, several scientific disciplines are involved.

It is obvious that a tool that allows a systematic approach to the analysis of these waste management systems would be useful.

OBJECTIVE OF THE ANALYSIS

The objective of the analysis of a waste management system for licensing purposes is usually the following:

- To credibly demonstrate that the optimum waste management design/site with acceptable environmental impact has been selected. This entails calculations, usually computer modelling, supported by sufficient field data to provide a reasonable degree of confidence in the model predictions.

There are three basic components to the meeting of this objective. The first is the development of feasible design options. The approach taken in developing these will vary depending upon the constraints under which they must be selected (e.g., decommissioning of an existing facility or design of a new facility). In the case of new facilities, a site selection process may be involved as well. The optimum option must then be selected.

The second component involves the gathering of sufficient field data to allow the prediction of the impacts of the waste management system and to allow a defensible analysis to support the choice of the optimum design.

The third component is to complete a credible analysis of the impacts of the waste management system to support the choice of the optimum design option/site and allow its licensing.

THE SYSTEMATIC MODELLING APPROACH

The purpose of a systematic modelling approach is to achieve the desired objective in a systematic and cost-effective manner. It can be used throughout a waste management project and is applied in three steps.

Step 1: A clear understanding of the behaviour of the facility-environment system is obtained.

One of the greatest benefits of modelling a waste management system results from the necessity to methodically simplify the system in order to model it. This forces the analyst to break the system down into its component parts and to consider the various mechanisms that lead to the release and transport of contaminants from the facility. From the subsequent modelling of each of the design options, information such as the following can be obtained:

- the water balance in the cover and the facility
- the contaminant release pathways requiring consideration
- the contaminant environmental transport pathways
- the contaminant exposure pathways
- the critical receptor
- sensitivity to design changes

The systematic modelling of the waste management system establishes a framework for further analysis, design, and field work.
Step II: Identify field data and design requirements.

From the results of the analysis in step I, and further sensitivity analysis of various components of the system and input parameters, information such as the following can be obtained:

- determination of the relative importance of the various release, transport and exposure pathways and the identification of the critical pathway(s),
- determination of the critical parameters
- definition of data gaps and requirements for a field and laboratory program
- screening of the effectiveness of design options and design components - identification of design constraints

These analyses should help to focus the data gathering and design activities.

Step III: Iterate steps I and II (along with more detailed field and design activities) until the objective is met.

The modelling and the design and field data gathering activities should proceed in parallel with continuous interaction between them. During this iterative process, an optimum design option with defensible analytical support can be identified.

The use of this approach in the early stages of a waste management project is cost-effective since more expensive field data collection and engineering design activities can be focussed. The understanding of the critical components of the waste management system is enhanced and the possibility of overlooking an important component of the impact is minimized. In addition, an organized framework is provided within which to carry out project activities right through to licensing.

CHINTKEX - A TOOL FOR THE APPLICATION OF THE SYSTEMATIC MODELLING APPROACH

CHINTKEX is a modular component model composed of a number of interacting stand-alone models that describe the various components of a waste management system and its surrounding environment. CHINTKEX was developed to provide a tool for the application of the systematic modelling approach to waste management systems. It can model the system as a whole or any of its component parts separately. CHINTKEX is readily adaptable to a wide range of waste disposal problems since it can easily be configured to use different modules in different sequences. The mathematical models it employs are all analytical and thus the computer time requirements are minimal and the model can be executed on a microcomputer.

CHINTKEX is a comprehensive model in that it describes all the major release, transport and exposure pathways that are normally associated with a waste management system. These are shown in figure 1. It can be used both to calculate the impact of chronic releases of contaminants and the impact of intrusion.

The various modules that comprise CHINTKEX are shown in figure 2. The major function of each of the modules is indicated on the figure. It should be noted that facility overflow, as may result from the "bathtub effect", can be accommodated by the model. In addition, radioactive decay chains such as the uranium decay chain, can be accommodated. In the Groundwater Transport Module, a series of interconnected aquifers and/or cracks can be modelled by the repeated application of the porous media and/or fracture models and the use of a program that calculates the net contaminant concentration profile. A similar calculation is possible in the Surface Water Module for a series of rivers and lakes.

FIGURE 1: PATHWAYS MODELLED BY CHINTKEX
Hydrological Module
Initiating events for contaminant releases
- Infiltration
- Runoff
- Erosion
- Contaminant release rates in runoff and eroded sediment

Facility Module
Behaviour of contaminants within waste area
- Water balance
- Cover thickness and integrity
- Contaminant inventory
- Contaminant leaching and release to ground and surface water

Air Release and Transport Module
Release of contaminants into air
- Gaseous release
- Wind-induced suspension of particulates
- Plume definitions
- Particulate deposition rates

Groundwater Transport Module
Behaviour of contaminants in groundwater
- Transport in fractures and porous media
- Concentrations
- Contaminant retardation

Surface Water Module
Dilution of released contaminants in surface water
- Contaminant concentrations in surface water

Food Uptake Module
Impact of released contaminants on food chain
- Contaminant concentrations in
  - meat
  - dairy products
  - vegetables
  - grain
  from water and air pathways

Human Uptake Module
Impact of released contaminants on humans
- Contaminant uptake by humans
- Dose from radioactive contaminants
- Dose from surface gamma radiation

FIGURE 2: THE CHIMTEX MODULES
APPLICATION OF THE SYSTEMATIC MODELLING APPROACH

Options for the Decommissioning of Uranium Tailings

This approach was applied to a study which investigated several alternatives for the decommissioning of uranium tailings in northern Saskatchewan. Various cover types and shapes were analysed as well as the removal of the tailings for disposal in an engineered pit. The CHINTKX model was used to determine the environmental impact of the various options. The various release, transport, and exposure pathways were modelled and the relative importance of each was determined for each of the options. The effect of the various designs on the long-term impact was calculated. Table 1 presents the relative costs and impacts from this analysis. During this study the various components of the objective described previously were met.

### TABLE 1: RESULTS OF MODELLING OF TAILINGS OPTIONS

<table>
<thead>
<tr>
<th>Option</th>
<th>Impact*</th>
<th>Cost*</th>
</tr>
</thead>
<tbody>
<tr>
<td>No Cover</td>
<td>X</td>
<td>Y</td>
</tr>
<tr>
<td>Shaped surface - no cover</td>
<td>X</td>
<td>8Y to 11Y</td>
</tr>
<tr>
<td>Cover 1 - Convex</td>
<td>X/5</td>
<td>20Y</td>
</tr>
<tr>
<td>Cover 1 - Concave</td>
<td>X/5</td>
<td>17Y</td>
</tr>
<tr>
<td>Cover 2</td>
<td>X/5</td>
<td>36Y</td>
</tr>
<tr>
<td>Cover 3</td>
<td>X/5</td>
<td>60Y</td>
</tr>
<tr>
<td>Relocation to pit</td>
<td>X/100</td>
<td>170Y to 225Y</td>
</tr>
</tbody>
</table>

* Cost and impact are in relative units

As a result of the CHINTKX modelling, the critical pathway for the various design options was found to be the seepage pathway (except for a nearby receptor group upstream of the tailings area for which the air pathway contributed the major impact). This information was valuable when considering the sensitivity of the predicted impacts to the various designs and the choice of the critical receptor groups.

Component II of the objective addressed the field data requirements. In this case, the critical pathways that were identified for the various receptor groups were used to select the parameters of importance. From a review of the existing data, data gaps could be identified and a field gathering program was set up to provide the necessary data. At this stage of analysis, sensitivity analysis on the various parameters also provided useful information for focusing the field data collection program.

Among the parameters identified to be important were the parameters governing the surface flows that provide input to the concentration determination for the seeping contaminants. The seepage path lengths and velocities, the retardation coefficients in the groundwater system, and the leachate concentrations in the tailings (especially for Th-230) were also important. In addition, information on the rate of radon emanation from different parts of the existing tailings was found to be necessary.

Component III of the objective addressed the analytical work necessary for discussions with and eventual licensing by the regulatory authorities. In the present application, the cost-effectiveness analysis performed on the various options, as well as the supporting modelling work, formed the basis for recommendations as to which options were not to be considered further and which options were investigated in further detail. These recommendations were accepted by the regulatory authorities.

Options for the Disposal of Uranium Refinery Wastes

The systematic modelling approach was also applied to an investigation of several alternatives for the disposal of uranium refinery waste. The alternatives included engineered burial and cavern disposal of the wastes at three different sites. Various design features of the engineered burial system were investigated during this study. The CHINTKX model was again used to analyse the various release, transport, and exposure pathways from the various options and to determine the resulting environmental impact. During this study the various components of the objective were also met.

Component I of the objective addressed the design options. The differences in the impacts between the various options were clearly identified and a clear understanding of the behaviour of the various waste disposal systems was obtained by the application of the CHINTKX model. Although no cost-effectiveness analysis was performed in this application, a basis for future cost-effectiveness analysis was established. There was no identifiable impact associated with the long-term releases of contaminants from cavern disposal facilities.
For the engineered burial option, the modelling indicated that the potential impact is very sensitive both to cover breaching and overflow of contaminants from the facility. This indicated that the use of an impermeable liner in the bottom of the facility was not advisable and thus it was removed from the design. The design of the cover of the facility should also minimize the probability of breaching by erosion (mainly gully erosion) and plant root or burrowing animal intrusion. These mechanisms may breach the cover and expose the wastes and would lead to an unacceptable environmental impact. The function of the cover as an infiltration barrier is also an important consideration in the long term. In this application, the use of the systematic modelling approach was an important tool in establishing design constraints for the engineered burial option.

As well as the identification of design constraints, CHINTEX was used to identify the critical pathways and thus identify the critical receptor group. In this particular application, it was found during the course of the analysis that the receptor selected initially was, in fact, not the critical receptors. Thus, it was necessary to change the location of the receptor. In connection with this, it was also necessary to consider an additional groundwater pathway.

Component II of the objective addressed the field data requirements. From the identification of the critical pathways, the important parameters could be selected. As in the case of the previous example, the data gaps were identified and the field data gathering program was set up to provide the necessary data. In this particular application, several iterations were made between modelling and the gathering of field data and the refinement of the design. The modelling approach proved to be very useful in directing those other activities.

Among the significant findings in the modelling of the engineered burial option were the following:

- the chemical contaminants were found to result in the greatest environmental impact
- the seepage of contaminants to the groundwater system was found to be the critical pathway
- parameters describing the effectiveness of the infiltration barrier of the cover were found to be very important
- parameters describing the water balance within the system were important
- parameters describing the subsurface transport and the dilution flows in the subsurface system were important
- parameters describing the leachate concentrations were found to be very important

Component III of the objective addressed discussions with the regulatory authorities. During this application, the modelling results and the CHINTEX modelling tool were reviewed in detail by the regulatory authorities. On the basis of the completed analysis and an examination of CHINTEX, there was general agreement on the value of the approach and the tool in the application described and significant input for further work in this area was obtained.

CONCLUSIONS

From the described applications of the systematic modelling approach using the CHINTEX model, it can be concluded that, in both cases, the approach was valuable in meeting the various components of the stated objective. The applications described represented a diverse spectrum of different waste management designs and encompassed both remedial projects and new facility designs. There was a clear focus of design and field data gathering work resulting from the use of the CHINTEX model and this resulted in substantial cost savings during the studies. In addition, the potential for overlooking a critical component of the environmental impact was minimized because of the clear understanding of the waste management systems gained as a result of the modelling and the subsequent identification of the critical pathways and receptors. Thus, it is anticipated that this approach will prove itself to be valuable in many different waste management problems in the future.
Concentrations of long-lived radionuclides have been compared in indigenous terrestrial and wetland plants (trees, shrubs, herbs and grasses) growing on inactive or abandoned uranium mill tailings sites in the Bancroft, Elliot Lake and Uranium City areas. Ra 226 and Pb 210 concentrations were determined in stems, leaves and roots of aspens and birches \(N = 50\); in samples from 2 shrubs and 3 grasses; in stems, fruits, leaves, rhizomes and roots of cattails \(M = 28\) and in fruits, stems, leaves, roots of sedges \(N = 8\). The biomass was dried at 75°C and wet oxidized. Radionuclide analyses were carried out at the University of Waterloo by H. Sharma.

None of the concentrations of radionuclides indicated that above ground biomass in the terrestrial and wetland areas are accumulating Ra 226 or Pb 210. Grasses from the Uranium City tailings (Gunnar Mines Ltd.) showed concentrations of radionuclides in the aerial parts to be one tenth to one quarter of that in the roots. The concentration ratios (leaves and stems/roots) ranged from 0.09 to 0.19 for Ra 226 and a consistent ratio of 0.16 for Pb 210.

In indigenous trees growing on Bancroft and Elliot Lake tailings slightly higher Ra 226 and Pb 210 concentrations can be found in the stems than in the roots. Average Ra 226 and Pb 210 concentrations range from 2.6 to 41.2 and 2.1 to 9.3, respectively, for stems; for roots the average ranges are 4.9 to 36.7 and 1.4 to 7.3, respectively. The concentrations in the roots were generally lower than in the tailings with concentration ratios (roots/tailings) ranging from 0.2 to 0.4 for Ra 226 and 0.7 to 0.6 for Pb 210.

The partitioning of Ra 226 between roots and stems resulted in concentration ratios (stems/roots) ranging from 0.77 to 1.6. For Pb 210 the distribution was very similar with concentration ratios ranging from 0.41 to 1.7 for trees.

Sedges and cattails in wetland areas on tailings were evaluated. Root/rhizome concentration ratios for cattails ranged from 0.04 to 0.24 for Ra 226; for Pb 210 the ratio was consistently 0.5. The stem/rhizome ratios for cattails were 0.5 and 0.1 for Ra 226 and Pb 210, respectively.

For the sedges, the stem/root ratio for Ra 226 ranged from 0.36 to 0.41 and for Pb 210, from 0.03 to 0.08. In general these ratios indicate that only one tenth of the concentrations in the roots or rhizomes reach the stems of the wetland vegetation. This clearly differentiates the distribution pattern of Ra 226 and Pb 210 between indigenous wetland and terrestrial vegetation growing on inactive or abandoned uranium mill tailings.

These differences in wetland and terrestrial radionuclide distribution in the waste site plant ecosystem may have important implications in evaluating specific environmental pathways of long-lived radionuclides from uranium mill tailings.

This work was supported by DSS contract #OSU81-00024 to the University of Toronto sponsored by the Atomic Energy Control Board and Environment Canada. The assistance of Rio Algom Ltd. and Denison Mines Ltd. is gratefully acknowledged.
A HIGHLY SELECTIVE METHOD FOR REMOVING NATURAL RADIOACTIVITY FROM DRINKING WATER

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ABSTRACT

High natural concentrations of uranium and radium have been found in well waters in the Lac du Bonnet area, Southeastern Manitoba. A filter system has been developed at the Whiteshell Nuclear Research Establishment (WNRE) that removes >90% uranium and >70% radium and may be installed in-line with any household water supply. This system will be available soon through selected distributors.

INTRODUCTION

In 1983 May, high levels of alpha and beta radioactivity were discovered in private well waters in the Regional Municipality of Lac du Bonnet in Southeastern Manitoba. Many of the waters were considered unsafe for long-term, continuous human ingestion because gross alpha activities exceeded 1 Bq/L. It was subsequently determined that this radioactivity was caused almost entirely by isotopes of uranium and radium. Using the research and design expertise at the Whiteshell Nuclear Research Establishment, Pinawa, Manitoba, a technique was developed to remove uranium and radium selectively from the drinking waters.

This paper briefly examines the causes of radioactivity in drinking water and existing methods of removal, and then describes the development and testing of a two-component system for the selective removal of uranium and radium from household water supplies.

RADIOACTIVITY IN GROUNDWATER

Radioactivity in groundwater may be either man-made, from waste discharges and atomic bomb fallout, or produced naturally, from the dissolution of gases and rock minerals. In the latter category, for most groundwaters, the radionuclides of concern are isotopes of uranium and thorium, and their daughter products (mainly $^{226}$Ra, $^{222}$Rn, $^{210}$Po and $^{210}$Pb). Some typical levels of uranium and radium in natural water are shown in Table 1.

Uranium occurs in groundwater mainly as the two isotopes in the $^{235}$U decay series, $^{235}$U and $^{234}$U, with small amounts of the isotope $^{232}$U, from the $^{235}$U decay series (see Figure 1). The amount of $^{234}$U relative to $^{238}$U varies, especially in groundwaters, and its radioactivity level often exceeds that of $^{238}$U because $^{234}$U, the decay product, is more easily leached from mineral surfaces. Although radioactivity ratios of $^{234}$U to $^{238}$U in natural waters are generally between one and three, values of up to twenty have been observed. (2)

Uranium is slightly soluble in natural waters, occurring mainly as the carbonate ion complexes $\text{UO}_2(\text{CO}_3)_2^-$ and $\text{UO}_2(\text{CO}_3)_3^-$, between pH 7 and 10. (3) For a typical groundwater under CO$_2$-rich, oxidizing conditions, the maximum solubility of uranium is about 1 g/L. (4), although natural groundwaters seldom attain this value.

Although four radium isotopes occur naturally in groundwaters, only $^{226}$Ra, and $^{228}$Ra have sufficiently long half-lives to permit appreciable concentrations to occur (Figure 1). The isotope $^{226}$Ra occurs in the $^{232}$Th decay series and has a relatively insoluble parent ($^{232}$Th) and a moderate half-life (5.8 years). It is, therefore, less mobile than $^{226}$Ra, which has a more mobile parent ($^{238}$U) and a longer half-life (1600 years).

Radium concentrations (expressed as radioactivity levels of $^{226}$Ra) are generally quite low (<1 Bq/L). Higher levels sometimes occur in brines and some near-surface fresh waters. In natural waters, radium exists mainly as the free ion, $\text{Ra}^{2+}$, or the uncharged complex, $\text{RaSO}_4$. Radium is readily absorbed from solution by clays and rock silicates but is stabilized by high concentrations of Ca$^{2+}$, Mg$^{2+}$ and Cl$^{-}$. (5)

EXISTING REMOVAL METHODS

Various techniques to remove uranium and radium from water have been developed and tested on a laboratory scale or on actual drinking water supplies. Table 2 summarizes these techniques and their

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### TABLE 1. TYPICAL CONCENTRATIONS OF URANIUM AND RADIIUM IN GROUNDWATERS (1)

<table>
<thead>
<tr>
<th>Location</th>
<th>Rock</th>
<th>U ($\mu$g/L)</th>
<th>$^{226}$Ra (Bq/L)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Eastern UK</td>
<td>sandstone</td>
<td>5</td>
<td>-</td>
</tr>
<tr>
<td>Bath, UK</td>
<td>limestone</td>
<td>0.05</td>
<td>0.4</td>
</tr>
<tr>
<td>Central USA</td>
<td>dolomite/sedst.</td>
<td>0.6</td>
<td>0.3</td>
</tr>
<tr>
<td>USA regions</td>
<td>various</td>
<td>120</td>
<td>1.1</td>
</tr>
<tr>
<td>S. Carolina</td>
<td>sedimentary</td>
<td>-</td>
<td>1.0</td>
</tr>
<tr>
<td>S.W. Sask.</td>
<td>sediments</td>
<td>240</td>
<td>-</td>
</tr>
<tr>
<td>E. Germany</td>
<td>sandstone</td>
<td>17</td>
<td>-</td>
</tr>
<tr>
<td>Okanaganan, B.C.</td>
<td>sedimentary</td>
<td>8000</td>
<td>-</td>
</tr>
<tr>
<td>Nevada</td>
<td>tuff</td>
<td>21</td>
<td>0.1</td>
</tr>
<tr>
<td>Minnesota</td>
<td>granite/gneiss</td>
<td>2</td>
<td>-</td>
</tr>
<tr>
<td>Nepal</td>
<td>granite</td>
<td>14870</td>
<td>1.0</td>
</tr>
<tr>
<td>Japan</td>
<td>granite</td>
<td>23</td>
<td>-</td>
</tr>
<tr>
<td>Scotland</td>
<td>granite</td>
<td>15</td>
<td>-</td>
</tr>
<tr>
<td>Stripa, Sweden</td>
<td>granite</td>
<td>90</td>
<td>1.3</td>
</tr>
<tr>
<td>Atikokan, Ont.</td>
<td>granite</td>
<td>10</td>
<td>0.1</td>
</tr>
<tr>
<td>Lac du Bonnet</td>
<td>granite</td>
<td>840</td>
<td>16</td>
</tr>
<tr>
<td>Manitoba</td>
<td>granite</td>
<td>-</td>
<td>-</td>
</tr>
</tbody>
</table>

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advantages and disadvantages. A detailed review of the methods is given by Fry et al. (6), Lee et al. (7), and Reid et al. (8). Unfortunately, some of the techniques have not had enough testing to identify all their characteristics.

TABLE 2. EXISTING METHODS OF REMOVAL OF URANIUM AND RADIUM FROM DRINKING WATER

<table>
<thead>
<tr>
<th>Method</th>
<th>Mechanism</th>
<th>Comments</th>
</tr>
</thead>
<tbody>
<tr>
<td>Distillation</td>
<td>vapour efficient, unselective,</td>
<td>low production</td>
</tr>
<tr>
<td>Reverse osmosis</td>
<td>membrane filtration,</td>
<td>- as above -</td>
</tr>
<tr>
<td>Lime softening</td>
<td>coagulation efficient, unselective,</td>
<td>large scale, expensive, requires pretreatment</td>
</tr>
<tr>
<td>Coagulation</td>
<td>floc formation removes U only,</td>
<td>large scale, requires pretreatment</td>
</tr>
<tr>
<td>Adsorbents</td>
<td>sorption on peat, charcoal, Al₂O₃,</td>
<td>removes U only, may add colour, needs low pH &amp; HCO₃ etc.</td>
</tr>
<tr>
<td>Cation resins</td>
<td>ion exchange for Ra</td>
<td>removes Ra only (U at low pH), adds Ra to water,</td>
</tr>
<tr>
<td>Po &amp; Mn removal</td>
<td>co-precipitation</td>
<td>removes Ra only, low efficiency, requires pretreatment</td>
</tr>
</tbody>
</table>

None of these techniques is ideally suited to uranium and radium removal from waters in the Lac du Bonnet region. No system will selectively remove uranium and radium simultaneously, yet be inexpensive, require no preconditioning of the water, and be practical for point-of-use installation.

An alternative technique, which does satisfy the above criteria, is described in this paper.

SELECTIVE REMOVAL OF URANIUM AND RADIUM

Gascoyne (9) showed that uranium could be extracted from limestone groundwaters using anion exchange resin, with no preconditioning of the groundwater. In situ tests showed that extraction efficiencies for eight sites were high (mostly > 70%). Jasper and Oldham (10) and Lee and Bondietti (11) subsequently demonstrated the efficient and selective uptake of uranium using anion resin, from well water in British Columbia and from low-level radioactive waste pond water in Tennessee, respectively.

Moore and Reid (12) and Moore and Cook (13) removed radium from seawater and fresh water, respectively, using a man-made fibre impregnated with manganese dioxide. The removal efficiency was 90% in several tests.

These two methods form the basis of the selective removal system described here. It was devised and tested in a series of experiments using groundwaters in the Lac du Bonnet area, Manitoba. A summary of the tests is described below.
METHOD

The radium filter was made by treating loose acrylic fibres with potassium permanganate to impregnate them with manganese dioxide. The uranium filter was prepared by pre-rinsing an anion exchange resin to remove excess amine, followed by conditioning with a brine solution. Each material was packed into a ~1-L polycarbonate, water-filter housing, modified with a central riser pipe and steel mesh for flow distribution and material retention. The units were interconnected in series and installed in the cold-water line of the kitchen sink tap of a household in the Lac du Bonnet area, in 1984 March. Over 5000 L of water were passed through the system at a rate of 2 L/min, over the following two months before materials were renewed. Samples were taken frequently during this period to measure uranium, \(^{226}\)Ra and major and minor elements. The radiation field arising from radionuclides trapped on the filters was measured in situ with a hand-held gamma monitor.

RESULTS

Variations in the radium and uranium concentrations over the duration of the test are shown in Figure 2. For the first 4000 L of water, removal efficiencies of radium and uranium were > 70% and > 90%, respectively. Radium removal was less consistent than uranium removal, possibly because of compacting of the fibre and channelling of flow during the test.

After ~ 4000 L of water had been passed through the filter, radiation fields on contact with the radium filter container were 0.018 mR/h, compared with 0.011 mR/h 30 cm away from the filter. Room background radiation was also about 0.011 mR/h. No radiation above background could be detected on the uranium filter. At this time, the filters had reached 60%, or more, of their final capacities.

Analyses of the filtered water to determine the effects on water quality showed that cation concentrations remained unchanged by filtration, throughout the experiment (see Figure 3). Anion concentrations varied only in the first ~ 200 L of water treated; after this, they were equal to raw water concentrations (Figure 3b). The main change in water quality was the removal of SO\(_4\) from the water and release of Cl\(^-\) from the resin. Iron, well water colour and turbidity were also removed, resulting in a much clearer drinking water. Tests showed that the iron was replaced by manganese, derived presumably by a redox reaction occurring in the radium removal fibre. Although dissolved manganese does not constitute a health problem, it may affect aesthetic quality by producing black deposits on fixtures over time. To some extent, this is countered by the prevention of iron staining. Several other trace metals were also removed by the system, including copper, zinc, barium and lead.

Results of continuing tests of the household system, at lower water demand rates, over the period 1984 May to 1985 November confirmed the results described above and demonstrated the suitability of the system for long-term use.

A commercial version is now available, developed by the Whiteshell Nuclear Research Establishment in conjunction with Water Conditioning of Canada Limited, Regina. This version features sealed, disposable filter cartridges containing the sorbing materials. The cartridges are designed for about one year of use in a household with normal drinking water demand (~ 2 L per person per day). The cost of the complete system should be significantly less than that for a distillation or reverse osmosis unit. Although radiation fields were not significant during operation, used filter materials should be disposed of properly, preferably in a municipal landfill site.

SUMMARY AND CONCLUSIONS

The uranium and radium removal system has been found to reduce uranium and radium in drinking water.
to well within Provincial and Federal guidelines. General water quality and mineral content of the water are not affected, and radiation fields associated with the system are negligible.

A commercial version has been developed by the Whiteshell Nuclear Research Establishment in conjunction with a water treatment company. It uses sealed, disposable cartridges containing the filter materials, and the entire unit costs less than a reverse osmosis or distillation system.

REFERENCES

(1) GASCOYNE, M., "High levels of uranium and radium in groundwaters at Canada's Underground Research Laboratory," Lac du Bonnet, Manitoba, paper in preparation, 1986.


SESSION D: FUSION I

Chairman: A.B. Meikle, CFFTP

Update on the construction of the Tokamak de Varennes.
G.W. Pacher, I.R.E.Q.

The potential in Canada for fusion by polarized nuclei.
D. Giusti, Consul-Tech Engineering Ltd. and D.G. Andrews, University of Toronto

Irradiation of lithium-based ceramics for fusion blanket application.

Two-dimensional model of current density and temperature in the T.F. coils of the Tokamak de Varennes during long pulse operation.

Concepts for fusion fuel production blankets.
P. Gierszewski, Canadian Fusion Fuels Technology Project
UPDATE ON THE CONSTRUCTION OF THE TOKAMAK DE VARENNES

G.W. Pacher, I.R.E.Q., Varennes, Quebec

ABSTRACT

The Tokamak de Varennes, Canada's contribution to tokamak-oriented fusion research, is to begin experimental operation in the autumn of 1986. This paper gives the status of construction at the time of the conference (June, 1986). The Tokamak de Varennes is a medium-sized tokamak experiment whose scientific vocation is the investigation of plasma-wall interaction and surface studies in a long-pulse mode. It is this program which has been the determining factor in machine design, leading, for instance, to demountable toroidal field coils, to an elaborate power supply for the ohmic heating system, to complex internal structures, to a complicated cooling system. Presently, the power supplies and primary cooling system are installed and commissioning is beginning. As far as the actual experimental machine, the tokamak, is concerned, support structures are in place and some of the coils have been positioned. The vacuum vessel has been received and is being prepared for vacuum tests and installation. The critical path for machine assembly depends on coil delivery, expected to be completed in the beginning of July. Low power tests of the power supplies will proceed in parallel with assembly of the tokamak, to be followed by low power commissioning of the experiment and first plasma in the autumn of 1986.

INTRODUCTION

The Tokamak de Varennes will form the kernel of the Canadian Centre for Magnetic Fusion Research, whose purpose it is to participate in international fusion research and to contribute at the forefront of this research in certain selected areas. In order to carry out this mandate, the device must have relevant parameters, program choices must be made that enable the center to compete and contribute significantly, and the device must be equipped with adequate equipment to carry out the program. This policy has resulted in a trade-off between the relevance of the device and the available budget, a trade-off whose result is the Tokamak de Varennes. Its preliminary design has been described in ref.1.

The scientific program of the Tokamak de Varennes, a medium-sized tokamak, is centered around the study of impurity transport and control, plasma-wall interactions, the effect of long pulses, and material studies. This program choice results from the fact that, even in a medium-sized machine, the plasma periphery is similar to that of a larger machine or a reactor. By concentrating the plasma flux using a divertor, power fluxes of several megawatts per square meter can be deposited on a divertor plate, a flux similar to that of INTOR. The main areas of study are then the equilibrium between plasma and wall and the behaviour of the wall and divertor materials.

The most cost-effective divertor configuration to achieve these aims is a divertor triplet close to the plasma, and thus inside and linking the toroidal field coils. One of the coil sets, either toroidal or poloidal field coils, therefore has to be demountable for assembly purposes. In a machine of this size (0.86m major radius, 0.25m plasma minor radius), it is advantageous to construct the internal coils necessary for divertor operation without demountable joints. The TF coil set is therefore demountable. As a result, the OH solenoid can also thread the TF coils. Its larger size then allows a larger flux swing, thus a longer plasma pulse.

The characteristic features of the Tokamak de Varennes are thus demountable TF coils conceived for long-pulse operation, internal divertor coils, a sophisticated OH system which can give plasma pulses at a high repetition rate in order to increase wall-loading during the toroidal field pulse. The materials to be studied are mounted inside the vacuum vessel on cooled structures.

DESCRIPTION OF THE MACHINE

During operation of the tokamak, a circular loop of plasma is generated inside the vacuum vessel by producing a current in the ionized working gas. The current is generated initially by electromagnetic induction due to a rapid change of current in the ohmic heating circuit. The location of the current channel and therefore of the plasma loop is defined by the various magnetic fields generated by the different coil sets described in the following sections, each of which is powered by its own power supply. The OH circuit exhibits the novel capability of rapid repetitive operation, up to once a second, in order to attain a high duty cycle plasma even in ohmic operation. The rapid rate of change of current is produced by a solid-state switch (plasma start up switch, PSUS). At the end of the current flat-top, the switch inserts a high resistance into the primary OH circuit, generating a negative voltage and driving the plasma current down rapidly, permitting transformer recharge and subsequent plasma restart.

In the following brief description of the machine, individual components are indicated by one- or two-letter codes in brackets, which are
indicated on figure 1 for clarity.

**Toroidal field coils (TF)**

The toroidal field of 1.5 Tesla at a major radius of 86 cm is produced by sixteen four-turn coils, carrying 97.5 kA per turn. Each turn consists of two roughly L-shaped sections, machined from plates of a chromium-copper alloy, and connected at the upper inner and lower outer corner by demountable joints. The coils are cooled by demineralized water flowing in cooling passages which have been gun-drilled in the plates. A more detailed description of the coils is to be found in ref. 2.

**External poloidal field coils (OH, EF)**

The poloidal field of the tokamak is produced by two sets of coils, the first of which is external to the vacuum vessel and consists of 21 coils. With the exception of four low-current coils which decouple the rapid feedback circuit from the ohmic heating induction (OH field), all of these coils thread the toroidal field coils.

The poloidal flux swing is coupled to the plasma by the air-core ohmic-heating (OH) transformer whose primary consists of a central solenoid and, fed in series with the solenoid, three supplementary pairs of coils (OH) arranged around the vacuum vessel. The geometry of the coils is optimized so as to minimize the stray field of the transformer in the plasma region.

The main equilibrium field (EF) is provided by two coil pairs fed in series with opposed current flow to produce a vertical field in the plasma region with essentially no coupling to the OH field. In order to increase the vertical stability of the plasma, two pairs of curvature-correction coils may be inserted into this circuit when desired. Finally, one coil pair (top and bottom with antiparallel current flow) is used to provide a radial field to control the vertical position of the plasma.

**Internal coils (DF, HP)**

Because the Tokamak de Varennes is to investigate plasma-wall interactions, the machine has been conceived with a double poloidal divertor system, created by two coil triplets (DF) inside the vacuum vessel above and below the plasma. Their construction has been previously described (ref. 3). Two additional internal coils (HP), similar in construction to the divertor coils but larger in major radius, are provided to give rapid position control of the plasma.
Vacuum vessel and pumping system (H.C.B.A)

The vacuum vessel (H) is a single-walled rectangular chamber made of 316L stainless steel. Major diagnostic access is provided by 14 rectangular ports (G) in the outer cylinder as well as by 11 trapezoidal ports in each of the top and bottom plates. The current path in the vacuum vessel is interrupted toroidally at two insulating gaps (B), sealed with Viton gaskets. In order to install the internal coils, the lid (A) of the vessel is removable. It is sealed to the vessel with double Viton O-rings which intersect the vertical Viton gaskets at the insulating gap. All of these seals are differentially pumped.

One of the horizontal ports is used to connect a rectangular pumping duct to two turbomolecular pumping groups. This system will provide an effective pumping speed at the vacuum vessel opening of nearly 2000 l/s. In order to control the gas pressure in the divertor chambers during the experiments, Zr-V-Fe getter pumping units will be installed with a pumping speed of 19000 l/s in the divertor chambers.

Support structures (F.C.M.I.J.K)

An important part of the machine are the structures required to hold the coils against the various forces exerted on them by gravity and by their magnetic fields (ref.4). The TF coils stand on a supporting ring (E) near their outer vertical branch, and on a central support (C) underneath their inner leg. Centering forces resulting from the interaction of the toroidal field with the TF coil currents are supported by a central column made of fiberglass (M) on which the TF coils press. The overturning moments on the TF coils resulting from the interaction of their currents with the poloidal field are taken up near their inner corner by transfer into an external torque frame and near their outer corner by struts (I) connecting one TF coil to the next. This arrangement reduces the bending moments on the TF coils near the inboard demountable joints. Near the inner corner of the TF coils, a casting (J) resembling a crown gear, whose teeth extend between the TF coils, transfers the moment to a stainless steel beam (L) above the machine. At a radius of 3 m, the beam is rigidly connected to an identical beam below the machine to complete the torque frame. As with the vacuum vessel, these structures contain two insulating gaps in the toroidal direction. In addition, odd currents in the massive supporting structures near the transformer throat due to the rapid change of the radial component of the OH field are reduced by inserting a fiberglass ring (K) as the connecting element between the crowns and the massive steel beams.

POWER SUPPLIES AND ELECTRICAL SYSTEMS

The various coil systems will be powered by six independent power supplies which draw their power from the 25 kV line of the high power laboratory of IREQ. With the exception of the HP and VP supplies, they are twelve pulse rectifiers, all solid state, capable of operation for at least one one-second pulse every five minutes, or one 30-second pulse every fifteen minutes. All power supplies except for the toroidal field supply allow inverse voltage to be applied to the coils in order to control the speed of current decrease in the coil systems. In the following sections, the characteristics of the individual power supplies are indicated.

TF Power Supply

Purpose: to provide the current for the toroidal field at a high pulsed level for the main plasma discharge and at a lower continuous level for the cleaning discharge.

Current: 100 kA 30 s on, 900 s off
100 kA 10 s on, 300 s off
120 kA 10 s on, 900 s off
10 kA continuous

Voltage: 293 V at rated current
Load Inductance: 2.4 mH
Load Resistance: 1.6 milliohm (cold), 2.1 milliohm (hot) (including .05 milliohm for busbars)

OH Power Supply

Purpose: to provide the primary current for the OH transformer system and the primary current variation necessary to maintain or change slowly the plasma current; four-quadrant operation.

Current: +/-20 kA 30 s on, 900 s off
+/-.24 kA 1 s on, 300 s off
+/- 2 kA continuous

Voltage: +/- 744 V at rated current
Load Inductance: 6.8 mH
Load Resistance: 11.5 milliohm (cold), 13.9 milliohm (hot) (including 1.5 milliohm for busbars)

Plasma Start-Up Switch (PSSS)

Purpose: to provide the rapid variation of primary current in the OH transformer which induces ionization of the plasma and initial plasma current build-up; also to induce a rapid decrease of the plasma current at the end of the plasma current flat-top. The primary current is rapidly varied by forced commutation from the switch into a resistance.

Current: +/-20 kA 30 s on, 900 s off
+/-.24 kA 1 s on, 300 s off
+/- 2 kA continuous

Voltage: +/- 15 kV at rated current
Load Inductance: same as OH
Load Resistance: same as OH

Switch characteristics:
Type: bidirectional, thyristor
Repetition: one interruption per second for 30 s
Resistance: .75 ohm max., 15 kV, consisting of 6 0.5 ohm units which can be configured to give .1/.125/.167/.25/.5/.75 ohms
Commutating Capacitor: .9 mF, +/- 15 kV

EF Power Supply

Purpose: to provide the current for the EF coil system which produces the vertical field providing plasma
equilibrium on the time scale much longer than the vacuum vessel time constant.

Current:
- 23 kA 30 s on, 900 s off
- 23 kA 1 s on, 300 s off
- 25.3 kA 10 s on, 900 s off

Voltage: +/- 421 V at rated current

Load Inductance: .73 mH

Load Resistance: 4.4 milliohm (cold), 6.8 milliohm (hot) (including 1.5 milliohm for busbars)

DF Power Supply

Purpose: to provide the current for the DF coil system which produces the field configuration of a double poloidal divertor.

Current:
- 16.6 kA 30 s on, 900 s off
- 16.6 kA 1 s on, 300 s off
- 18.3 kA 10 s on, 900 s off

Voltage: +/- 133 V at rated current

Load Inductance: .04 mH (two systems in parallel)

Load Resistance: 2.3 milliohm (two systems in parallel) (including 1 milliohm for busbars)

HP Power Supply

Purpose: to provide the current for the HP coil system internal to the vacuum vessel which produces a vertical field providing radial plasma equilibrium on a time scale of the order of the vacuum vessel time constant.

Current:
- +/- 6 kA 30 s on, 900 s off
- +/- 6 kA 1 s on, 300 s off

Voltage: +/- 800 V at rated current

Load Inductance: .22 mH (two systems in parallel with maximum decoupling)

Load Resistance: 3.4 milliohm (two systems in parallel) (including 1 milliohm for busbars)

VP Power Supply

Purpose: to provide the current for the VP coil system which produces the radial field providing control of the vertical position of the plasma.

Current:
- +/- 1 kA 30 s on, 900 s off
- +/- 1 kA 1 s on, 300 s off

Voltage: 8 V

Load Inductance: .04 mH

Load Resistance: 1.6 milliohm (including 1 milliohm for busbars)

ASSEMBLY SEQUENCE

After installation of the base plates for the support and torque frame on the concrete platform which carries the tokamak, the central support (C) and the supporting ring (E) are assembled. The lower beam (L), fiberglass ring (K), and crown (J), are threaded onto the central support and deposited on the concrete platform. The inner legs of the first five TF coils are placed on the central support and on the support ring. The OH solenoid is placed on these TF coil legs, and the other eleven inner TF coil segments are then threaded into the solenoid throat one by one and placed on the support structures. In the next step, the lower poloidal field coils are placed on the lower TF coil arms and their supports are assembled. The vacuum vessel is inserted, followed by the upper PF coils. The lower torque structure can now be brought into position, so that the bus bars and the cooling hoses underneath the machine can be installed. Once the outer legs of the TF coils are assembled to the inner legs and the demountable joints are bolted up, the upper portion of the torque frame and the struts (I) between TF coils can be assembled, followed by the installation of the upper bus bars and coil cooling connections. Initial assembly and operation of the machine will occur without the coils internal to the vacuum vessel. Their installation will require demounting the TF coils and removing the lid (A) of the vacuum vessel. Essentially everything below the vacuum vessel can stay in place for this later installation.

ASSEMBLY STATUS

At the beginning of June, the primary cooling system is installed and being commissioned. The power supplies and their primary power source are installed and ready to begin logic checks. Five TF coils are mounted on the supporting ring ready to receive the solenoid. The rest of the support structure has been delivered and can be installed according to the above assembly sequence. The vacuum vessel is presently being prepared for vacuum tests at site. Coil delivery is expected to be completed in the beginning of July. Low power commissioning tests of the power supplies will start in August and proceed in parallel with machine assembly. First plasma is expected for late autumn 1986.

ACKNOWLEDGEMENTS

The Tokamak de Varennes is a joint project of the National Research Council Canada and Hydro-Quebec carried out by Institut de recherche d'Hydro-Quebec, Institut national de la recherche scientifique, University of Montreal, MPB Technologies and Canatom Inc. Members of these institutions participated in the scientific definition and the conceptual design of the experiment, in the implementation of the control systems and the diagnostic apparatus, and will participate in the experimental operations. Federal funding for operations is to be provided via Atomic Energy of Canada, Ltd. P.B.Cumyn directed the mechanical engineering of the device. G.Barbec was followed by R.O.Butler in directing the electrical engineering. Assembly is proceeding under the direction of A.Chamberland of Hydro-Quebec, Groupe Equipement, Direction Equipement de Production.
REFERENCES


(3) P. B. CUMYN, "Tokamak de Varennes, the Design of its Internal Poloidal Field Coils", Proc. 6th Annual Conf. Canadian Nuclear Society, Ottawa, 1985, p. 10.8

THK POTENTIAL IN CANADA FOR FUSION BY POLARIZED NUCLEI

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ABSTRACT

Present-day approaches to fusion power have used conventional methods, involving expensive installations and calling for financial support at the billion-dollar level. It has not therefore been possible for Canada to develop an independent fusion power programme at this level; however, the production and supply of tritium fuel is seen as a useful activity. In this paper it is proposed to use spin-polarized nuclei as a means of closing the fusion fuel cycle within Canada. Longer lifetimes permit the use of simpler and less-expensive plant.

INTRODUCTION

The experimentally-proven creation of stabilized spin-polarized atomic hydrogen gas (IH) at cryogenic temperatures by L.F. Silvers et al. (1,2), and by H.W. Cline et al. (3,4) in 1980 opened the door to the application of spin-polarized nuclei to the production of energy in fusion plasmas. Kulscrud et al. (5) theoretically demonstrated that the depolarization lifetimes are greater than the reaction lifetimes required for the reactants in fusion reactors. They also pointed out other advantages, such as damping of microinstabilities, and the fact that “the polarized nucleus of a hydrogenic atom is not depolarized by ionization”. It was also possible to suppress unwanted reactions, for instance polarizing the nuclei of a D-He plasma parallel to the magnetic flux B could be used to suppress the D-D reaction, giving preference to the D-He reaction, which is neutron-free, and could lead to a cleaner reactor. Although optical pumping methods could be used to feed polarized atomic hydrogen gas (6), we believe that injection through hexapole magnetic fields will be more appropriate. Again, spin-polarized atomic hydrogen could be used as a plasma source to replace multiparticle ion acceleration in the case of neutral beam injection (7).

The next step in the process is the application of the developed techniques to deuterium. Work is proceeding at the Massachusetts Institute of Technology (8); however, there is already a strong Canadian interest in this area, with ten years of experience. There is an opportunity for development of an efficient, general-purpose, Canadian neutral-beam injection system, so that a state of readiness exists. This system can be initially developed by using spin-polarized hydrogen and then upgrading to polarized deuterium. The groundwork for this latter step has been laid in Canada by the University of British Columbia. B.W. Statt et al. (9) measured properties at spin-polarized atomic hydrogen at temperatures below 1 K, and estimated constants for a and b (proton up and down) decays, with separate temperature dependences of the recombination rate constants, at densities in the range 1 to 5 x 10^20 m^-3. Reynolds et al. (10), used electron spin resonance (ESR) at 14 GHz to measure three-body recombination rates and two-body surface relaxation rates for doubly spin-polarized H between 100 and 200 mK (0.1 - 0.2 K). Their results have been in line with the previously-mentioned MIT studies (8). Their work has continued with the same equipment (11) and has provided a sound basis for Canadian use of their techniques in an approach to fusion power. However, a necessary intermediate step is to apply their procedures to study spin-polarized deuterium D4+. So far, preliminary results for D4+ show three electron spin-polarized states of D4+, labelled a, b and c, being the lowest and c the pure electron spin-polarized state, analogous to the b state of IH. What is most important, however, is their production of D4+ at densities of the order 1 x 10^20 m^-3 (as compared with IH 2 x 10^21 m^-3) (12). Decay times have also been measured for doubly spin-polarized D4+, time-constants being of the order of 30 minutes, as compared with 7 days for IH+

THEORETICAL ASPECTS

As has been indicated, IH is gaseous atomic hydrogen and can be produced at cryogenic temperatures. Stabilization requires symmetric orientation of the electron spin vectors and it is necessary to slow down the molecular recombination which would otherwise produce explosive energy releases. Atomic IH interacts via a triplet potential 3(IH) (symmetric electron spin state), which is strongly repulsive, in contrast with the singlet potential 1(IH) (antisymmetric electron spin state), which is highly attractive. Figure 1 shows these potentials. The triplet potential has a shallow (repulsive) well at an internuclear distance,
regarding properties, the spin is a vector quantity and possesses both magnitude and direction. The hydrogen atom contains two particles, the proton (spin \( \frac{1}{2} \)) and the electron (spin \( \frac{1}{2} \)), so that the H atom can exist in four spin states described by the Hamiltonian (14):

\[
\hat{H} = \frac{\mathbf{S} \cdot \mathbf{B}_0}{2} + \frac{a}{2} \cdot \mathbf{S}
\]

(1)

where \( a = a \cdot \mathbf{S} \cdot \mathbf{B}_0 \).

The hyperfine energy levels in high magnetic fields are given by the eigen-equations of state (14):

\[
\begin{align*}
\Delta E_a &= \frac{\alpha}{2} \cdot \mathbf{S} \cdot \mathbf{B}_0 \\
\Delta E_d &= \frac{\alpha}{2} \cdot \mathbf{S} \cdot \mathbf{B}_0
\end{align*}
\]

(2)

where \( \Delta E_a \) and \( \Delta E_d \) represent the splitting of the Zeeman states and the magnetic field gradient.

The control of molecular-recombination is crucial to the use of spin-polarized hydrogen or deuterium in neutral-beam injection and in fusion reactions. The control of molecular-recombination is crucial to the use of spin-polarized hydrogen or deuterium in neutral-beam injection and in fusion reactions.
and \( K_s \) = intrinsic surface rate constant, \( \lambda v_s \),
\[ A/v = \text{surface-to-volume ratio}. \]
\( \Delta = \text{de Broglie thermal wavelength of atom}, \]
\( \epsilon_B = \text{adsorption energy of atom on surface}, \]
\( v_s = \text{mean relative thermal speed onto the surface}, \]
\( \lambda = \text{collision mean free path in two dimensions}. \)

Therefore measurement of \( K_s \text{eff} \) as a function of temperature will provide a measure for the saturation density of H atoms on the surface, and will also provide a value for \( \epsilon_B \) from the equation:

\[ n_{s\text{sat}} = \frac{A}{V} \text{vol} \exp \left( \frac{\epsilon_B}{kT} \right) \]  (9)

where \( n_{s\text{vol}} = \text{volume density of hydrogen} \).

It is clear that the density of a system containing polarized hydrogen atoms in the \( \alpha \) - and \( \beta \)-states will decay rapidly at first, and then continue to decay until all the \( \alpha \)-state atoms are depleted. Figure 2 shows a typical curve (4). It was taken at 300 mK and 11 T. In the experiment, the H source was turned on (A). Then it was turned off (B) and the density decayed rapidly as \( \alpha \)-state atoms recombined. The system was now essentially \( \beta \)-state (C) and it decayed slowly by nuclear relaxation. The sample was destroyed (D) and the system relaxed (E) to the same density as (C). Depletion of \( \alpha \)-state atoms implies that if recombination is to occur, then the remaining doubly-polarized atoms in the \( \beta \)-state (H+I) have parallel electron and nuclear spins and must go through a nuclear spin relaxation process, which will transfer atoms from the \( \beta \) to the \( \alpha \)-state (I). Recombination now becomes a function of the rate of transfer (T-1). Experimentally (4), the rate of transfer is observed to be proportional to the total gas density, given by:

\[ T^{-1} = G(n_B + n_A) \]  (10)

Relaxation can occur both on the surface and in the gas (4).

The coefficient \( G \) can be written:

\[ G = G_s + G_{\text{eff}} \]  (11)

where \( G_s \) = relaxation rate in gas, proportional to \( T^4 \),
\[ G_{\text{eff}} = G_s(A/v)^2 \exp \left( 2 \frac{\epsilon_B}{kT} \right) \]  (12)

Thus it is evident that as the relaxation rate on the surface is important for the low-temperature process, because of the exponential temperature dependence, the gas-phase relaxation process becomes predominantly important at high temperatures, for which the constant \( G \) has only a weak temperature dependence. For fusion plasmas, the question arises as to how long the atoms can be trapped in the magnetic field. In the absence of any recombination or relaxation mechanism, a plasma containing trapped H+ will escape by evaporation from the potential well with a time constant \( \tau_B \) by:

\[ \tau_B = \tau_0 \exp \left( \frac{\mu B}{kT} \right) \]  (13)

where \( \tau_0 = \text{mean time for atoms to escape from a location where field is zero} \)

of the order of a few millic on.

Experimentally (3), Equation 13 has been found to apply for short times and magnetic fields < 7 T. For longer times and magnetic fields > 7 T, the recombination process begins to take over and the experimental curve deviates from the theoretical curve (Equation 14), so that \( \tau_B \) (experimental) at 10 T and 0.3 K is approximately two orders of magnitude less than the theoretically predicted 10³ sec.

APPROACH TO FUSION WITH SPIN-POLARIZED NUCLEI

Now that the properties of spin-polarized H+ are becoming well-known, and that techniques for its production have evolved to the level of commercialization, its application to the fusion process can be considered in global terms. It is well known that a fusion fuel consisting only of hydrogen has very little chance of application to the fusion process, mainly on account of high energy demand. The most promising fuels for fusion reactors must therefore contain other hydrogenic species. The easiest fuel to ignite is a D + T mixture:

\[ D + T \rightarrow (\text{HI} = 3.52 \text{ MeV}) + (\text{H} = 14.06 \text{ MeV}) \]  (14)

Existing fusion reactors are not yet equipped to handle these high energies and neutron loads, therefore current experimentation uses hydrogen (H) for its abundant availability, even though the energy multiplication factor \( Q \) is less than 1. Temperatures suitable for plasmas - millions of degrees upward - do not occur naturally on earth, at least not in bulk form. Material walls are unsuitable, therefore magnetic and electric fields are used for plasma confinement. The Lawson criterion requires the product \( n \times \tau \) of plasma density \( n \times \text{confinement time} \) \( \tau \) to be more than 10²⁰ m⁻³ sec. Typically a \( n \) of = 3 x 10²⁰ m⁻³ and a \( \tau \) of > 0.3 sec would qualify. Since the cryogenic lifetime of polarized hydrogen could be as much as 10⁶ sec, then it is reasonable to foresee a good performance in the fusion plasma. The magnetic fields used to confine the plasma present a problem regarding adequate plasma heating and steady-state operation of the reactor. A current remedy of interest is the injection of energetic neutral beam, having the appropriate attenuation lengths, which will then heat up the plasma by elastic collisions, while becoming a plasma themselves. For successful and efficient refuelling of the reactor and heating of the plasma, the neutral beam must penetrate deep into the plasma without being ionized. A recent study by
European scientists indicates that an adequate performance of current neutral beam injectors is to be anticipated (17). When neutral beam injection was first examined, the \( V \times B \) electric field was considered in order to polarize the ions so as to overcome mismatch of Larmor radius of the trapped \( D^+ \) and the injected \( D^+ \) particles (18) (in current neutral beams these particles constitute 30\% of the beam composition) and to take advantage of the independence of the spin coordinates and the coordinates of configuration space, in order to overcome the magnetic barrier. However, in current beam research, the requirement has shifted from high density to high flux. The \( H_\parallel \) or \( D_\parallel \) sources will operate at lower densities - less than 10^2 m^-3 - and the previously described major recombination mechanisms will become less important. Confinement times of a few seconds will be adequate, up to say 12 electron-spin impurity. The process of energizing polarized neutral beams can be greatly improved if use is made of their magnetic dipoles.

As these particles can be accelerated by proper orientation of their dipoles into magnetic fields, they are not subjected to the local extraction potentials and are therefore not limited by Child-Langmuir constraints (19). As a result, a single uniform large plasma source can be used, as opposed to existing multi-hole and expanded plasma boundary sources.

Pursuant to the studies of Rook et al. (20) and Ad'yaev et al. (21,22,23) on spin-polarized D^+T reactions, Kulsrud et al. (5) demonstrated that for the spin-polarized D^+T reaction there is also a reactivity enhancement equivalent to 3/2 \( f \). According to Humber (24), the magnitude of the quantity \( f \) is a function of the magnitude and phase of the density matrix element with total angular momentum \( J = \frac{3}{2} \).

\[ f = 1 \text{ for reactions with pure } J = \frac{1}{2}, \]
\[ f = 1 \text{ for reactions having } J = \frac{1}{2}. \]

As mentioned in the Introduction, Kulsrud et al. also suggested (5) that it is possible to control reaction products by for instance preferentially selecting reactions leading to helium, rather than to neutrons. It is also possible to improve magneto-hydrodynamic stability and to increase the fraction of \( \alpha \)-particles trapped into well-defined and confined orbits. Klepper's group at MIT took up the production of polarized deuterium as a fusion fuel (25). Canadian interest in neutral beam injection has existed since 1974. Project Fusion Canada's report dated November 1974 considered that if a superior Canadian neutral beam injector would be developed, it would find worldwide application. The authors feel that the capability exists within Canada for the development of powerful spin-polarized neutral beam injectors suitable for fusion reactors.

**Selection of Fusion Reactor Type**

Current research favours the Tokamak. Canadian Fusion Fuels Technology Project (CFFTP) have looked at this device in a tentative way as a candidate for a Canadian project (29). However, there are many problems remaining to be solved. Canada would be committing heavy expenditures if a premature attempt was made to come in. M. Hugnet reports (26) that a number of inherent vices in Tokamaks remain to be eliminated, including the production of very large forces due to interaction of toroidal field currents with plasma and toroidal coil magnetic fields. These forces are much increased during pulses and during plasma disruptions and positional instabilities. Typically, on JET, the twisting moment on the toroidal magnet increases rapidly from 10,000 Mg-m just before a disruption to 20,000 Mg-m just after. Also, vertical instabilities result in fast displacement of the plasma. In one case an instability at 2.7 MA caused the plasma to move down 1 m in 20 msec, to collide with the wall, collapse in disruption and impose a pulse force of 250 Mg on the vacuum vessel. Canada lacks the resources to deal with problems such as these.

It is therefore proposed that Canada investigate the magnetic mirror type of linear machine, Fig. 3, as a partner for spin-polarized fuel and neutral beam injection.

The equation describing motion of charged particles, from a given inertial frame of reference, in this system is

\[
m \frac{d^2r}{dt^2} = q (\frac{dr}{dt} \times \vec{B} + \vec{E}) + \vec{F}
\]

where \( m \) = relativistic mass, \( m \), charge = \( q \), magnetic induction = \( \vec{B} \), electric field strength = \( \vec{E} \) and non-electromagnetic force = \( \vec{F} \).

The resultant motion is periodic. If a charged particle is injected with pitch angle \( \alpha \) at a point \( P \) where intensity of magnetic induction is \( \vec{B}_i \), then it will travel in a helix along the field-line, with a resolved parallel velocity of
neutral atoms to ions in the plasma and still maintain good attenuation lengths. For a plasma distribution isotropic in $\phi$ and $\theta$, the azimuth and angle with respect to the neutral beam, the relative collision velocity for the evaluation of rate coefficient $<\sigma v>$ depends only on $\beta$, according to Ref. 19. This situation does not take from the usefulness of the spin-polarized beam in other types of fusion reactor, but rather provides a unique opportunity to secure the benefits of collective angle and relative velocity matching.

Relating to impurity control, there appear to be fewer problems with the magnetic mirror machine. Impurities can escape through holes in the magnetic field and can be reduced by proper selection of beam entrance angle. Another advantage with mirror machines relates to $\beta$, the ratio of plasma pressure to magnetic pressure. The value of $\beta$ should be high, in order to obtain the most product from a given set of magnets, hence to minimize equipment changes per unit product. The higher the $\beta$, the lower the total unit energy cost once the system is in full operation. The value of $\beta$ for a typical mirror machine easily exceeds that for a Tokamak. The major problem with the mirror machine has been to offset the lower temperatures with their lower reaction cross sections. However, with spin-polarized beams transferring more of their energy directly to the ions in the plasma, there are good prospects of solving the problems within Canada's boundaries.

**CONCLUSIONS**

Spin-polarized hydrogen and deuterium nuclei could point the way for Canada to close the fusion fuel cycle within its own boundaries. Suitable sources and beam injectors should soon be brought to the development stage. Because of the combination of better beam angle matching, adequate residence times, better utilization of the magnetic fields, high nuclear densities and relatively long lifetimes, it is more advantageous to use the magnetic mirror system over the Tokamak.

**ACKNOWLEDGMENTS**

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**REFERENCES**


(8) GREYTAK, T.J., Personal communication, 1984.


(12) HARDY, W.N., Personal communication, April 7, 1986.


IRRADIATION OF LITHIUM-BASED CERAMICS FOR FUSION BLANKET APPLICATION


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ABSTRACT

Unvented CREATE tests have shown that, under reducing conditions, most of the tritium (greater than 70%) is released from LiAlO₂ and Li2O as HT or T₂; the balance as HTO or T₂O. Residual tritium is very small, less than 0.02%. Varying the sweep gas composition has a dramatic effect on the form of tritium released. With a quartz extraction tube during post-irradiation heating, a He sweep gas results in 10-30% release as HT or T₂; with a He-1%H₂ sweep gas, greater than 60% release as HT is achieved. The effect of extraction tube material is also significant. Using pure He sweep gas, a quartz extraction tube results in 10-30% release as HT or T₂; stainless steel produces 80-95% as HT or T₂. Chalk River and CEA (Saclay) - fabricated LiAlO₂ behaved similarly to that from ANL in these tests. The first vented test at Chalk River, CRITEC-1, planned for 1986/87, will examine ANL-fabricated LiAlO₂, 0.3 wt% Li-6, 30 mm ID, 40 mm OD annular pellets, in a six-month irradiation at 670-1170 K, varying the sweep gas, with on-line HT/HTO measurement.

INTRODUCTION

There is currently an effort by the international fusion community to test and evaluate solid breeder blanket options. Major topics include material selection, optimization of microstructure and blanket operating conditions, and tritium release characteristics. The FINESSE study (1) recently reviewed the world effort on irradiation testing. In Canada, the program is based on the long-term experience with ceramics, irradiation testing and tritium handling at Chalk River Nuclear Laboratories (CRNL). French (2), U.S. (3), and Japanese (4) in-reactor tests have previously examined individual aspects of extraction vessel material and sweep gas composition on the form of tritium recovered from LiAlO₂ and Li₂O. Fischer and Johnson (5) expressed the effect of experimental conditions on the form of the released tritium in terms of oxygen activity on the thermo-dynamic interrelationship in a breeder system. This paper gives details of tritium behaviour in lithium-based ceramics in unvented tests, and outlines a major instrumented test under preparation for 1986/87.

EXPERIMENTAL

Unvented Capsule Tests

The unvented capsule tests, designated CREATE (Chalk River Experiment to Assess Tritium Emission), are those that do not have tritium release instrumentation (thermocouples and flux detectors can be included). Tritium release information is obtained after the irradiation is complete, and the capsule is removed from the reactor. Capsule tests can measure the interaction between the ceramic and the cladding material - thought to be a problem, especially between LiAlO₂ and certain cladding materials. Also pellet swelling, cracking, and grain size and pore size changes can be observed, as can the amount of tritium remaining in the ceramic. Information can also be obtained on the form of tritium released, as a function of capsule material and sweep gas.

Maximum sample size is 2 cm diameter and 15 cm long. Typically, samples weighing 50-100 mg are cut from sintered pellets of the ceramic for irradiation. Each sample is vacuum-annealed in a quartz tube, and sealed in the tube for irradiation without further exposure to air. LiAlO₂ samples are annealed at 670 K and 3 x 10⁻² Pa for 1 h; Li₂O samples at 870 K and 3 x 10⁻² Pa for 6 h. Samples are then irradiated for 48 h at an average flux of 7 x 10¹⁶ n.m⁻².s⁻¹(thermal) and 7 x 10⁻¹⁵ n.m⁻².s⁻¹(greater than 1 MeV).

The apparatus used to recover the tritium is shown schematically in Figure 1. Table 1 gives sample characteristics. The free tritium recovered...
TABLE 1: CREATE SERIES SAMPLE CHARACTERISTICS

<table>
<thead>
<tr>
<th>EXPERIMENT</th>
<th>MATERIAL</th>
<th>DENSITY % OF THEORETICAL</th>
<th>ENRICHMENT ATOM% Li</th>
<th>GRAIN SIZE* μm</th>
<th>POLE DIAMETER μm</th>
</tr>
</thead>
<tbody>
<tr>
<td>CREATE-II</td>
<td>LiAIO₂</td>
<td>65</td>
<td>7.5</td>
<td>0.05-0.3</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>with some clusters of 1 μm grains</td>
<td></td>
</tr>
<tr>
<td>CREATE-III</td>
<td>LiAIO₂ pellet fabricated from as-received powder</td>
<td>64</td>
<td>-</td>
<td>0.2-1</td>
<td>1.29</td>
</tr>
<tr>
<td></td>
<td>LiAIO₂ pellet fabricated from powder ground in isopropanol</td>
<td>64</td>
<td>-</td>
<td>0.1-1</td>
<td>0.29</td>
</tr>
<tr>
<td>CREATE-IV</td>
<td>LiAIO₂</td>
<td>75</td>
<td>50</td>
<td>2-10</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td>Li₂O</td>
<td>90</td>
<td>90</td>
<td>4-15</td>
<td>-</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td>with some larger grains, 30-40 μm</td>
<td></td>
</tr>
</tbody>
</table>

* from qualitative SEM examination

Tritium are determined. The tritiated water may include T₂O and HTO, and the reduced tritium T₂ and HT, but for simplicity only HTO and HT are used in this paper to refer to these tritium forms. The HTO is removed in the first ethylene glycol bubbler and the sweep gas then passes through an ionization chamber, which provides on-line monitoring of the HT released. A second measurement of the HT is obtained by passing the sweep gas through a CuO bed to convert the HT to HTO, and another set of ethylene glycol bubblers to remove the HTO. The total integrated release of both HT and HTO is determined by analyzing the bubbler solutions using liquid scintillation counting. The time dependence of only the HT release is obtained from the ionization chamber readings.

Both He and He-12 H₂ can be used as sweep gases at a flow of 0.5 L/min. The He is purified by passing it through a hot titanium bed; the He-12 H₂, by passing it through a Deoxo unit and a molecular sieve drier. The oxygen and moisture contents of the purified gas are less than 1 μL/L.

Extraction tubes constructed from quartz, stainless steel, Inconel-600 and nickel are available. Tests are generally performed at 873 K for 4 h. Tritium remaining in the ceramic after annealing is recovered by dissolving the sample in 6 N HCl, neutralizing with 6 N NaOH and distilling the resulting solution. Tritium in the distillate is determined by liquid scintillation counting.

**Ventilated Capsule Tests**

The vented capsule tests, designated CRITIC (Chalk River In-Reactor Tritium Instrumented Capsule) permit continuous in-capsule monitoring of the tritium release from the ceramic during the irradiation, by passing a sweep gas around or through the ceramic and into an analysis train. Since fusion reactors will probably use sweep gas to recover the tritium in the same way, the experiment attempts to model a miniature segment of a blanket. Figure 2 shows a diagram of the CRITIC assembly. A sample size 4 cm diameter by about 10 cm long is possible in the current capsule.

**FIGURE 2: CRITIC-I ADVANCED TRITIUM RECOVERY TEST ASSEMBLY**

Tritium is generated in the ceramic by neutron-induced fusion of lithium, and migrates to the ceramic surface. Flowing sweep gas collects the tritium released from the lithium ceramic and passes...
FIGURE 3: CRITIC-I TRITIUM ANALYSIS TRAIN

through a tritium analysis system to determine the release rate and tritium form. Figure 3 gives details of the analysis train. The ceramic is typically in the form of sintered pellets, with about 20% porosity. The capsule provides approximately uniform ceramic temperatures to facilitate analysis of release data; a small radial temperature gradient of about 50 K enables calculation of thermal conductivity of the ceramic. The temperature is adjustable between 400 K and 1200 K, which covers the range of expected operation in a commercial reactor, by varying the composition of an insulating gas layer (gap gas).

The form and rate of tritium release may be affected by factors such as surface impurities on the ceramic (particularly H2O and CO2), the composition of the sweep gas, and the materials chosen for the capsule walls. Therefore, the ceramic must be carefully fabricated and loaded into the capsule to minimize contamination by air. The sweep gas composition can be varied (He to He-1% H2) during the course of the experiment to determine the influence of composition. The capsule will be fabricated from nickel-based alloys, rather than stainless steel, to reduce container influence on the form of the tritium. Nickel-based alloys also provide more strength at high temperatures. To minimize condensation of HTO in the sweep gas lines, the gas lines will be trace-heated to about 300 K between the reactor and the tritium analysis system.

It is possible to measure important fundamental parameters so that the behaviour of other blanket assemblies can be inferred: diffusion, desorption, and heat transfer coefficients can be calculated as a function of temperature.

In addition to on-line tritium analysis, gamma spectroscopy monitors the release rate of trace quantities of other radioactive species. These are expected both from neutron activation and from fission of uranium impurities in the ceramics. A movable spectrometer will be located at the glove box containing the tritium analysis system, and a portable spectrometer will be available adjacent to the gas line exit ports from the reactor. Other instrumentation will include thermocouples, on-line flux monitors, and integrated flux monitors. Analysis of the gap gas permits measurement of the permeation rate of tritium through the Inconel capsule wall.

There are differences in neutronics between CRITIC and a blanket in a future fusion reactor. In CRITIC, tritium production will occur almost exclusively from thermal neutron captures by 6Li. In a fusion reactor, tritium will be generated from captures by both 6Li and 7Li, of neutrons within a wide energy range, 0-14 MeV. In both cases, helium will also be produced, and the stoichiometry of the ceramic will change. The average tritium production rate per gram of ceramic in a blanket will not be far different from the rate in CRITIC. Also, in a breeder material in a fusion reactor, displacement damage from energetic neutrons will be larger than in CRITIC. Near the first wall of a fusion reactor, the displacement damage rate will likely be larger by two orders of magnitude. Near the blanket rear, they will be comparable. A burnup of 0.3-0.5% total Li is anticipated in CRITIC.

The first vented test at Chalk River, CRITIC-I will examine ANL-fabricated Li2O, 0.3 wt% 6Li, 30 mm ID, 40 mm OD annular pellets, in a six-month irradiation at 700-1200 K, varying the sweep gas, with on-line HTO measurement. Li2O conductivity and tritium permeation will also be measured. Test operation is scheduled for 1986/87 for this BEATRIX test.

RESULTS AND DISCUSSION - CREATE TESTS

General

The total tritium recovered, which includes the free tritium, the tritium released during the 4 h
anneal and the residual tritium in the ceramic, ranged from 0.37 to 1.11 GBq per gram of LiAlO₂ for samples with natural ⁶Li enrichment, and from 7.4 to 16.6 GBq per gram of ceramic for Li₂O and LiAlO₂ with high ⁶Li enrichment. These generally agreed with neutronlc predictions. Burnup was approximately 0.05 atom% ⁶Li, calculated from the total tritium yield. The amount of free tritium and residual tritium was generally less than 1% of the total for the LiAlO₂ in CREATE-II and -III. The free tritium increased to 15% for the highly enriched samples, possibly as a result of more ⁶Li(n,α)²H reactions near the surface.

Form of Tritium Recovered

Table 2 summarizes the effect of the sweep gas composition and the extraction tube material on the form of the tritium recovered. Although HT and HTO proportions vary for samples tested under the same conditions, some general trends emerge. The HT/HTO ratio will also have been influenced by residual moisture in the ceramic, the sweep gas, and the experimental system. Generally, with either He-1% H₂ sweep gas or a stainless steel extraction tube, oxygen activity was very low and tritium was recovered primarily as HT, whereas primarily HTO was recovered when oxygen activity was raised by use of pure He sweep gas or relatively inert extraction tubes.

Tritium Release Mechanisms

A number of authors (3, 4, 6, 10) have investigated kinetics and release mechanisms of tritium from lithium ceramics in post-irradiation and in-situ tritium recovery tests and the importance of material characteristics and experimental conditions has been emphasized. Both solid state diffusion and desorption from the surface of the ceramic have been identified (11) as important to the release of tritium into the sweep gas stream. Although, the CREATE experiments were not specifically designed to study tritium release mechanisms, release data were compared to the following models for diffusion and desorption.

(a) Diffusion from spherical grains:

Crank (12) gives the fractional release (f) as:

\[ f = 1 - \frac{6}{\pi^2} \sum_{n=1}^{\infty} \frac{1}{n^2} \exp\left(-\frac{2n^2t}{a^2}\right) \]  

for the initial and boundary conditions, which can be applied to tritium release into the sweep gas stream, of \( C(r,0) = C_0 \), \( C(r,t) = C_s \) \( \frac{\partial C}{\partial r} |_{r=a} = 0 \) for \( r = 0 \) and \( C(0,t) = C_s \), where \( C \) is the concentration of diffusing tritium in the grain, \( r \) is the distance (0 ≤ r ≤ a), \( a \) is the grain radius, \( D \) is the diffusion coefficient and \( f = (C_0 - C_s)/(C_0 - C_s) \).

Late stage release can be expressed by

\[ f_{\infty} \rightarrow 1 - \frac{6}{\pi^2} \exp\left(-\frac{2n^2t}{a^2}\right) \]  

as only the first term of the series contributes.

<table>
<thead>
<tr>
<th>CERAMIC</th>
<th>SWEEP GAS</th>
<th>EXTRATION VESSEL</th>
<th>PREDOMINANT FORM</th>
<th>% HTO</th>
<th>PREDOMINANT FORM</th>
<th>% HTO</th>
</tr>
</thead>
<tbody>
<tr>
<td>Li₆O₂ (CREATE-II)</td>
<td>He</td>
<td>Stainless Steel</td>
<td>HTO</td>
<td>67-87 (2)*</td>
<td>HT</td>
<td>63-81 (2)</td>
</tr>
<tr>
<td>Li₆O₂ (CREATE-III, from as-received powder)</td>
<td>He</td>
<td>Nickel</td>
<td>HTO</td>
<td>51 (1)</td>
<td>HT</td>
<td>85-88 (3)</td>
</tr>
<tr>
<td>Li₆O₂ (CREATE-IV)</td>
<td>He-1% H₂</td>
<td>Stainless Steel</td>
<td>HTO</td>
<td>47-50 (2)</td>
<td>HT</td>
<td>80 (1)</td>
</tr>
<tr>
<td>LiAlO₂ (CREATE-IV)</td>
<td>He-1% H₂</td>
<td>Quartz</td>
<td>HTO</td>
<td>80 (1)</td>
<td>HT</td>
<td>80 (1)</td>
</tr>
</tbody>
</table>

* Number in brackets is the number of samples tested under the given conditions.
the various samples. Material characteristics such as grain size, pellet density and porosity, and variation in $^{6}$Li enrichment, as well as the material and experimental system conditions are all factors which may have contributed to observed tritium release kinetics.

Further details of CREATE-II, -III and -IV are given elsewhere (14).

**CREATE-V, French LiAlO$_2$ Under BEATRIX Program**

Preliminary results show:

- 40-90 mCi of tritium were recovered per gram of LiAlO$_2$; the variation is due to the different irradiation positions.
- 0.5% of the total tritium was recovered as free tritium.
- 0.5% of the total tritium was recovered as residual tritium when the ceramic was annealed at 873 K for 4 h; increasing to 23% when the ceramic was annealed at 773 K for 4 h.

The form of the tritium recovered is shown in Table 3. Generally, the tritium release rate and HT/HTO ratios were comparable to that obtained for the previous LiAlO$_2$ pellets which had similar microstructures. The previous experiments had shown that with He sweep gas and a stainless steel extraction vessel, the predominant form of the tritium was HT. The large amount of HTO measured for Samples 1, 2 and 5 (Table 3) is attributed to an oxidized stainless steel tube and a partially oxidized titanium sponge bed (used to purify the He). The use of a new stainless steel tube resulted in less HTO as did using the new tube and a new Ti sponge bed (Sample 6, Table 3).

For Sample 12, the flow of the He/0.1% H$_2$ sweep gas through the ceramic was reduced to 0.1 L/min, but He was added just prior to the ionization chamber to have a total flow of 0.5 L/min through the chamber. The reason for the large percentage of HTO has not yet been determined; under the test conditions, HT was expected as the predominant form. The addition of 0.1% H$_2$ to He enhanced the fractional release rate of HT.

Apparent diffusion coefficients were calculated from the HT fractional release curves for the runs carried out with He/0.1% H$_2$ at 870 K, 820 K and 770 K. The results are plotted in Figure 4 along with those given by other investigators. A band is shown for the CREATE-V data to depict a difference in the apparent diffusion coefficient when it is calculated from $f = 0.4$ or $f = 0.7$. The activation energy determined is very similar to that obtained by other investigators.

**CONCLUSIONS**

The effect of the oxygen activity of the experimental system on the form of the tritium recovered from LiAlO$_2$ and Li$_2$O was demonstrated. With He-$^{12}$O or a stainless steel extraction tube, oxygen activity was low, and tritium was recovered primarily as HT. With pure He and more chemically inert extraction tube, tritium was recovered primarily as HTO. There was, however, significant variation in the HT/HTO ratio for samples tested under similar conditions indicating the importance of possible material and system impurities on the results.
TABLE 3: CREATE V: FORM OF TRITIUM RECOVERED

<table>
<thead>
<tr>
<th>SAMPLE</th>
<th>TEMP. (°C)</th>
<th>FLOW (L/min)</th>
<th>SWEEP GAS</th>
<th>EXTRATION VESSEL</th>
<th>PREDOMINANT FORM</th>
<th>% HTO</th>
<th>% HTO-0.1% H2</th>
<th>% HF</th>
<th>NOTES</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>870</td>
<td>0.5</td>
<td>0</td>
<td>U</td>
<td>HTO</td>
<td>74</td>
<td>-</td>
<td>-</td>
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</tr>
<tr>
<td>2</td>
<td>870</td>
<td>0.5</td>
<td>0</td>
<td>I</td>
<td>HTO</td>
<td>71</td>
<td>-</td>
<td>-</td>
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<tr>
<td>3</td>
<td>870</td>
<td>0.5</td>
<td>0</td>
<td>I</td>
<td>HTO</td>
<td>74</td>
<td>-</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>4</td>
<td>870</td>
<td>0.5</td>
<td>0</td>
<td>I</td>
<td>HTO</td>
<td>74</td>
<td>-</td>
<td>-</td>
<td></td>
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<tr>
<td>5</td>
<td>870</td>
<td>0.5</td>
<td>0</td>
<td>I</td>
<td>HTO</td>
<td>74</td>
<td>-</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>6</td>
<td>870</td>
<td>0.5</td>
<td>0</td>
<td>I</td>
<td>HTO</td>
<td>74</td>
<td>-</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>7</td>
<td>870</td>
<td>0.5</td>
<td>0</td>
<td>I</td>
<td>HTO</td>
<td>74</td>
<td>-</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>8</td>
<td>870</td>
<td>0.5</td>
<td>0</td>
<td>I</td>
<td>HTO</td>
<td>74</td>
<td>-</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>9</td>
<td>870</td>
<td>0.5</td>
<td>0</td>
<td>I</td>
<td>HTO</td>
<td>74</td>
<td>-</td>
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</tr>
<tr>
<td>10</td>
<td>870</td>
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<td>0</td>
<td>I</td>
<td>HTO</td>
<td>74</td>
<td>-</td>
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</tr>
<tr>
<td>11</td>
<td>870</td>
<td>0.5</td>
<td>0</td>
<td>I</td>
<td>HTO</td>
<td>74</td>
<td>-</td>
<td>-</td>
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</tr>
<tr>
<td>12</td>
<td>870</td>
<td>0.5</td>
<td>0</td>
<td>I</td>
<td>HTO</td>
<td>74</td>
<td>-</td>
<td>-</td>
<td></td>
</tr>
</tbody>
</table>

NOTES

He added just before ion chamber for a total flow through chamber of 0.5 L/min.

FIGURE 4: APPARENT DIFFUSION COEFFICIENT FROM CREATE-V; OTHER LITERATURE DATA FOR COMPARISON

Preliminary data from French LiAlO2 under the BEATRIX Program show results comparable with those for previous CREATE tests. CRITIC-I, an advanced in-reactor tritium recovery test is planned for 1986/87.

ACKNOWLEDGEMENTS

LiAlO2 for CREATE-V test was provided by CEA, Saclay, France, under BEATRIX International Breeder Exchange Matrix, sponsored by IEA.

REFERENCES


(6) WISWALL, R.H. and WIRING, E., BNL-19766 (1975).


ABSTRACT

To prepare for the different operational modes of the toroidal field (T.F.) power supply, we have calculated the equipotentials, current density and isotherms of a T.F. coil using a 2-d finite element algorithm. The results show that, for the model we have considered and in the absence of cooling, the hottest point will reach a temperature of 90°C in 12 s at maximum current, well below a maximum temperature of 105°C permissible for the insulator. The ramp-up and ramp-down phases are 2 s each (equivalent to 1 s at full current), but leaves 10 s or 30 s of operation, respectively, at maximum current. The cooling reduces the central leg average temperature to 70°C and relaxes the temperature gradients at the joint boundaries, leaving the hottest point in the joint nearly unaffected.

INTRODUCTION

The Tokamak de Varennes will allow some important aspects of the operation of high-duty-factor machines to be investigated. It was therefore specifically designed to study a quasi-continuous operating mode, characterized by a series of successive plasma pulses, and a continuous mode (up to 30 s) using non-inductive current drive. These modes imply operating the T.F. coils in a quasi-steady state. Demountable T.F. coils allow complete access to the internal components of the tokamak, vacuum vessel and poloidal coils, for maintenance or replacement. The requirement that the T.F. coils be readily demountable, with minimum impact on T.F. coil performance, dictates the use of water-cooled resistive copper coils (1). The continuous operation of the T.F. coils is only limited by the amount of heat evacuated by the cooling system. The design of the demountable coils has used the experience acquired on the ISX, DOUBLET and FBX tokamak experiments.

THE T.F. COIL SYSTEM OF THE TOKAMAK DE VARENNES

The T.F. circuit, consisting of a T.F. power supply and sixteen T.F. coils, produces a quasi-steady toroidal confining field of 1.5 T for the plasma.

The T.F. power supply provides pulsed current at high intensity for the toroidal field during the main plasma discharge and at a lower intensity for the cleaning discharge. The design criteria of the different operational scenarios for the T.F. power supply are the following:

- 120 kA 10 s on 900 s off
- 10 kA continuous

The sixteen four-turn T.F. coils of the Tokamak de Varennes, symmetrically placed about the machine axis, present an angular separation of 22°30'. Each turn, carrying a current of 97.5 kA, consists of two roughly L-shaped sections, machined from 29 mm thick plates of work-hardened chromium-copper alloy CDA 187 and connected at the upper inner and lower outer corners by demountable joints. The outboard joint serves as the transition for the current from one turn to the next. The cross sections of the horizontal and outer vertical legs are rectangular, while for space limitations the cross section of the inner leg is reduced and assumes a trapezoidal form, increasing the current density and the heat load in that section.
The T.F. coil characteristics are:

- Conductor length per coil: 22.1 m
- Current per turn: 97.5 kA
- Total current per coil: 390 kA
- Conductor cross section: inner leg 3750 mm²; other legs 5100 mm²
- Current density: inner leg 2.6 kA/cm²; other legs 1.9 kA/cm²
- Coil resistance at 40°C: 100 micro-Ω/m
- Power per coil at 40°C: 1 MW
- Copper mass per coil: 844 kg

2-D FINITE ELEMENT MODELING OF A T.F. COIL TURN

The stationary potential and current distributions in a T.F. coil are entirely defined by the conductivity of the medium and the boundary conditions. In the case of a stationary flow, the potential obeys Laplace's equation. The coil material is homogeneous, except for silver plating of the contact surfaces of the joint. The problem is reduced to 2-D by projecting the coil thickness on the coil plane and solving the 2-D Laplace's equation with a variable conductivity proportional to the coil thickness. The 2-D current distribution serves to calculate the source term of a 2-D heat conduction equation. Since the conductivity in the potential equation depends on temperature, we have to solve a system of two 2-D coupled equations. The potential equation has mixed boundary conditions: a given potential difference between the coil entrance and exit and zero normal component of the potential gradient everywehere else on the boundary. In order to keep the total current constant, the potential difference has to be adjusted as the temperature increases. Since there is no heat loss other than water cooling in the model, the normal component of the temperature gradient is zero everywhere on the boundary.

This system of two coupled equations is solved numerically in real geometry using the 2-D finite element program TcondF (2,3). The mesh number is adjusted to insure a minimum precision of 1% on the potential and temperature distribution while the energy balance is controlled within 0.2% of error.

The demountable joint is too complicated to be modeled accurately in 2-D. In order to take into account its contribution to the total heat dissipation, we have adopted a simple two-parameter model: one parameter to adjust its resistivity, the other its temperature dependence. These parameters will be optimized with temperature measurements from the coil. The joint resistivity has been evaluated by comparison with measurements from other demountable joints on tokamak coils and its value has been fixed at five times the copper resistivity over the joint length. Its temperature dependence has been assumed equal to that of copper, a conservative assumption in view of the fact that the contact resistivity decreases as the contact pressure increases with temperature. While this model might underestimate the joint behavior at low temperatures, it is expected to overestimate it at high temperatures. In this model, the joint resistance is distributed over the entire joint region by changing the conductivity. Clearly,
the normal current density component and the
tangential electric field component are conserved
at the edges of the joint region.

The water cooling channels are modelled by
subtracting their volume from the thickness
projection in the potential equation. In the
projected channel area, we add a sink term in the
heat conduction equation representing the heat
transfer from copper to water as a volume effect.
The heat transfer coefficient for turbulent flow
takes into account the water temperature, the
water velocity in the channel, and the channel
diameter, disregarding any water velocity or
temperature distributions in the channel.

The value selected for the copper
resistivity is the highest value measured on the
coils. In this simple model we have neglected
the dependence of the resistivity on the
temperature gradient, the Thomson effect, and,
more important, on the external toroidal field,
the Hall effect. These effects, complicated to
model, would both act favorably on the current
distribution, playing the role of a centrifugal
force in the corners.

Different improvements are considered for
future implementation. Even though negligible
for the present operational modes of the
experiment, the eddy currents in the inner legs
induced by the changing of magnetic flux of the
solenoid may be simulated by adding a factor to
the current density. Provision has been made in
the calculation of the heat transfer coefficient
for a fouling factor, but since we start with
brand new coils this factor is not presently
taken into account. Finally, in order to take
into account correctly the heat transfer
phenomena, we have to include the water
temperature increase affecting the total heat
transfer from the copper. A simple calculation
showed that the water temperature increased by 6°C
during its passage through the T.F. coil.

ANALYSIS OF RESULTS

HALF COIL WITHOUT COOLING AND JOINT. Away from
the corner where the current is homogeneous, we
obtain, after 12 s, a temperature increase of
20°C in the horizontal and outer vertical legs
and an increase of 50°C in the central leg.

In the corners without joints, two hot points
appear. In the external corner, the temperature
distribution is symmetrical and a temperature
increase of 39°C is reached in the middle of the
corner. In the internal corner, a temperature
increase of 60°C is reached at a hot point
located along the inner boundary of the central
leg; the maximum temperature gradient is located
at the edge of the corner. Compared to the
symmetrical outer corner, this hot point
displacement is mainly due to two causes: the
current density is higher in the central leg than
in the horizontal leg and the heat flux is
greater to the upper corner where the copper mass
is higher. This displacement is favorable since
it helps to reduce the temperature and
temperature gradient near the joint and tends to
equalize the corner temperatures, reducing
thermal stresses.

FIGURE 3 ISOOTHERMS AFTER 12 s

IN Internal CORNER WITH JOINT BUT WITHOUT COOLING.
The equipotential distribution in the upper
corner ensures a homogeneous current distribution
in most of the legs; only near the corner is the
current distribution non-uniform. As expected,
the current density is maximum at the inside edge
of the corners (Fig. 4).

FIGURE 4 EQUIPOTENTIALS IN THE UPPER CORNER

In the central and horizontal legs, this
reduced model reproduces the above results. In
the joint region, as expected, high temperature
gradients and a hot point appear. The hot point,
at the joint bottom, presents a temperature
increase of 60°C; the horizontal temperature
gradients at the joint lateral boundaries are
much stronger than the vertical ones inside the
joint (Fig. 5).
**Internal Corner With Joint and Cooling.**

The effect of cooling is different in the different T.F. coil regions. In the central leg, the temperature is reduced from an average increase of 48°C to an average increase of 27°C, but temperature gradients appear between the regions of the cooling channels and the rest of the coil; the minimum temperature increase near a cooling channel is 22°C while the maximum increase between them is 30°C. The hottest point along the inner boundary of the leg has disappeared, its temperature increase is reduced from 60°C to 40°C; the proximity of the main cooling channel explains this drop. In the joint region, the hottest point is nearly unaffected by the cooling due to its distance from two medium flow cooling channels; its temperature increase is 59°C. At the joint boundaries, those two cooling channels help to relax the temperature gradients (Fig. 6).

**Conclusion**

These results assure us of the T.F. coil thermal behaviour during a 10 s flat-top pulse with a safety margin. The T.F. coil measurements during the first operation phase and the cooling system upgrade characteristics will provide a basis to extend the calculations to 30 s.

**Acknowledgement**

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**References**


CONCEPTS FOR FUSION FUEL PRODUCTION BLANKETS

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ABSTRACT

The fusion blanket surrounds the burning hydrogen core of the fusion reactor. It is in this blanket that most of the energy released by the DT fusion reaction is converted into usable product, and where tritium fuel is produced to enable further operation of the reactor. Blankets will involve new materials, conditions and processes. Several recent fusion blanket concepts are presented to illustrate the range of ideas.

INTRODUCTION

The blanket surrounds the burning hydrogen core of the fusion reactor. It is in this blanket that most of the energy released by the DT fusion reaction is converted into usable product, and where tritium fuel is produced to enable further operation of the reactor. Energy removal and tritium production capabilities will be needed for the next generation of fusion devices in the late 1990's, and certainly for subsequent demonstration reactors (1). However, the technology is in a very early stage of development, and will involve new materials, conditions and processes.

There are three general factors that affect the blanket design: reactor application, reactor conditions and material choice. Once these are defined, often based on larger engineering trade-offs or design choices, the particular blanket can be designed.

The applications are primarily electric power and fissile fuel production. However, there are other options to take advantage of the high quality neutron and plasma energy available, including isotope production, synfuel production, nuclear waste burnup, and space propulsion. In most cases, it is in the blanket where these functions are carried out, so the choice of product bears directly on the blanket design.

The reactor conditions are the reactor-related environmental conditions that affect the blanket behavior, particularly the neutron wall load, surface heat load, pulse cycle, lifetime irradiation fluence and magnetic field strength. These conditions vary according to the nature of the device (e.g., experimental test reactor, compact power reactor), and between reactor types (e.g., tokamak, mirror, reversed-field pinch) (2,3). Experimental devices are generally pulsed with relatively low neutron exposures. Since these devices do not have to produce economic product or be tritium self-sufficient, the blanket can operate at lower temperatures and pressures than would be acceptable in a commercial reactor.

The preferred confinement system has not been identified yet, although tokamaks have been the most studied and are the most successful devices so far. However, their low magnetic field utilization efficiency, pulsed operation, and complex geometry have sustained a search for improved tokamaks and for new confinement systems. The latter include reverse-field pinches, with more efficient use of the magnetic field in a more compact device, and tandem mirrors, with simple linear geometry. Alternatively, inertially-confined reactions have been studied where the reaction occurs as a series of small explosions (as in a gasoline engine) initiated by laser or particle beam pulses. The choice of reactor type influences the geometry, magnetic field, and the peak power conditions, although the average fluence, neutron damage, and temperature limits are generally set by more fundamental materials limits.

The material choices are the third major influence on the blanket. The blanket is generally composed of a tritium breeding, coolant, structure, and a neutron multiplier/moderator. There may also be additional material such as fertile fuel for conversion to fissile fuel. Table 1 is a partial list of materials of current interest (1,2,3,4). Although not all combinations are sensible (e.g., liquid lithium breeder with water coolant), there are still a large number of possible combinations, each with its own advantages and problems.

The factors involved in blanket design are illustrated below through a discussion of several recent concepts. Present blanket concepts for fusion-electric applications are typically based on helium-cooled pellets of lithium-bearing ceramics (1,2,5), flowing liquid metal systems with lithium

<table>
<thead>
<tr>
<th>Breeder</th>
<th>Coolant</th>
<th>Structure</th>
<th>Multiplier</th>
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</thead>
<tbody>
<tr>
<td>Liquid metal</td>
<td>Liquid</td>
<td>Austenitic</td>
<td>Beryllium</td>
</tr>
<tr>
<td>Solid ceramic</td>
<td>metal</td>
<td>steel</td>
<td>Lead</td>
</tr>
<tr>
<td>Molten salt</td>
<td>Helium</td>
<td>Ferritic</td>
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</tr>
<tr>
<td>Aqueous salt</td>
<td>Water</td>
<td>Refractory</td>
<td>alloy</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>Martensitic</td>
</tr>
</tbody>
</table>

TABLE 1: PRIMARY BLANKET MATERIAL OPTIONS
or lithium-lead as the coolant and breeding medium (2,5), and water-cooled modules containing slowly circulating lithium-lead (1). Alternate concepts based on molten salt cooled systems have been proposed, particularly for fusion-hybrid applications (2,6). Water-based concepts offer good cooling with a relatively conventional technology (2,4). The conditions in inertially confined reactors are substantially different because of the short-pulse, low vacuum conditions. Blanket concepts here are typically based on 'waterfalls' of liquid metal or solid pebbles to protect the structure and capture the neutron energy (7). Other recent ideas attempt a more efficient conversion of the neutron kinetic energy into electrical energy than through a Carnot-limited thermodynamic power cycle (8).

HELIUM-COOLED SOLID BREEDER BLANKET FOR ELECTRIC POWER

Solid lithium ceramic breeder blankets bear similarities to conventional fission reactor technology. The breeder is a solid compound with limited chemical reactivity and a high melting point. Consequently, the likelihood and results of accidents are limited. The solid breeder is generally compatible with attractive structural materials over temperature ranges of interest. The coolants can be conventional water or helium, both of which do not interact with the magnetic field. Also, many of the important development issues can be accomplished in existing facilities, primarily fission reactors.

Of all the solid breeders, Li2O may be able to provide net tritium breeding without the additional complexity of a multiplier. This advantage can only be realized in a blanket design that minimizes non-breeding absorption of neutrons. Therefore, it is most appropriate with helium coolant. The advantages of helium coolant are its chemical inertness, neutron transparency, lack of phase change, and high temperature capability. Helium is nonmagnetic and nonreactive. It is used as a heat transfer medium for fission reactors. The principal disadvantage for all gas coolants is their low volumetric heat capacity. This leads to operating the helium with pressures of several MPa. The pumping power also tends to be large (2-5% of blanket thermal power) because of the relatively low heat transfer coefficient and the consequent need for high flow velocities.

Figure 1 shows a helium-cooled solid breeder blanket concept for commercial fusion-electric conditions of 5 MW/m² neutron wall load, 1 MW/m² surface heat load, and a 3 year lifetime (2). Since the coolant is relatively high pressure, a lobed first wall mechanical design is used to retain the pressure, although it decreases the space available to breeder and other blanket material. The breeder is placed in a plate geometry, with helium flowing through narrow gaps between the plates, in order to maximize the breeder content. In this example, a net tritium breeding rate (TBR) of 1.1 is achieved. This TBR is slightly more than is believed to be required, but does not offer much margin for present uncertainties.

Stainless steels are compatible with helium, but are limited by decreasing mechanical strength under irradiation to about 550°C. The first wall has a high surface heat flux across it, and must be a few millimeters thick to provide some erosion allowance. In order to stay within the first wall peak temperature limits, the cold inlet helium is first directed around the curved first wall, and then directed into the blanket interior to cool the breeder.

Lithium oxide, although it may lead to simple designs without multipliers, is hygroscopic and thus more difficult to handle and fabricate. It tends to sinter, creep, and have high vapor pressures at relatively low temperatures (500°C). All solid breeder ceramics have poor thermal conductivity, typically 1-2 W/m-K for the 85% dense sintered product material. Thus, plates of only 1 cm thick are possible before reaching the temperature limits at the front end of the plate with peak heating rates of 40 MW/m³. This heating rate drops exponentially into the blanket.

The tritium generated in the solid breeder can be removed, in principle, by batch processing the solid breeder, through permeation into the main helium stream, or by a separate breeder helium purge stream. A 1000 MWe fusion reactor would produce about 120 kg tritium per year. Consequently, blanket modules would have to be removed frequently to minimize the tritium inventory and related costs and hazard. Frequent replacement of blanket modules for reprocessing would require far too much down time. On-line refueling as in CANDU reactors leads to a more
mechanically complex blanket with a substantial penalty in tritium breeding due to the increased structure and reduced coverage.

In-situ recovery by a separate helium purge stream flowing through the porosity in the solid breeder is the preferred approach. Tritium generated in the breeder diffuses to the grain surface, is desorbed by isotopic exchange with a small amount of H2 in the purge stream, and is swept away to a tritium recovery system external to the blanket. Since this is not the primary heat transport system, the helium purge does not need to run at high pressures and flow rates, simplifying the tritium recovery and control systems. Tritium recovery from the primary coolant is possible, but requires the extraction system to operate on a hot, high flow rate, high pressure coolant while minimizing tritium losses through permeation across the steam generator (5).

Helium-cooled solid breeders are attractive with respect to maintenance (easy spill cleanup, modest leak tolerance, relatively simple sector removal); safety (no chemical reactivity); and high thermodynamic efficiency (36%).

SELF-COOLED LITHIUM BLANKET FOR ELECTRIC POWER

The use of the same liquid metal as both tritium breeder and coolant greatly simplifies both design and materials considerations since the blanket requires only a structural material and a non-structural coolant/breeder. There is no coolant/breeder compatibility concern, although the chemical reactivity of the liquid metal is. Heat removal is simplified since the heat is deposited directly into the coolant and does not need to be conducted through regions of low thermal conductivity. Liquid metals offer high boiling points, so can provide excellent thermal efficiencies at lower pressures than water.

Both lithium and a lithium-lead eutectic (Li1Pb93) offer relatively high tritium breeding rates. Since the tritium is generated in the coolant, it is automatically circulated to outside the reactor without requiring a separate tritium purge system. It is easier to recover tritium from lithium, but the inventory is lower and the tritium breeding higher in lithium lead. Lithium-lead is also less chemically reactive, particularly with water which may be required for cooling nearby high heat flux components (2).

The temperature of the liquid metal is generally limited by compatibility of the coolant and the structure. Vanadium alloys may allow higher temperature mechanical strength as well as improved compatibility with the liquid metal. The pressure drop of a liquid metal flowing across a magnetic field is substantial, and increases as the channel wall thickness increases. Because of the complex geometry of most fusion devices, it is not possible to orient the liquid metal entirely parallel to the field. Consequently, the blanket must operate at reasonably high pressures (a few MPa at least), and sustain these pressures through limited thickness structural members. Furthermore, the pressure drop is only tolerable if high flow rates, such as required at the first wall, only occur in regions parallel to the field. This complicates the mechanical design.

An attractive liquid metal blanket is illustrated in Figure 2 (2). This is a lithium-cooled, lithium-breeder blanket with vanadium structure. The lithium flows slowly in large channels through much of the blanket, across the magnetic field. In the vicinity of the first wall, the channels are smaller and parallel to the field to allow rapid flow with minimum pressure drop.

Tritium is soluble in lithium, which limits the vapor pressure of tritium gas and consequently the tritium permeation from the coolant out of the primary heat transport system. It can be reasonably extracted and maintained to about 1 appm based on, for example, molten salt exchange processes. This blanket has exit temperatures of 500°C, and an estimated net thermal efficiency of 42%.

FIGURE 2: LIQUID LITHIUM SELF-COOLED TOKAMAK BLANKET FOR ELECTRIC POWER PRODUCTION
WATER SELF-COOLED BLANKET FOR NEAR-TERM EXPERIMENTAL DEVICES

Water is an excellent coolant with substantial industrial experience. Its primary drawbacks for power production are the high pressures and lower thermal efficiencies achievable. Reasonable water-based blankets have been proposed for commercial reactor applications (1,2).

Next-generation experiments will have neutron wall loads of about 1 MW/m² over sufficiently long pulses to achieve thermal equilibrium. Adequate cooling is required. The main blanket need not necessarily operate at commercial conditions, although some test blankets would. It may be more useful to have the bulk of the blanket and shielding functions performed by as simple, reliable and conventional a technology as possible for example, with water cooling and stainless steel structure.

Figure 4 shows that a blanket based on a small amount of lithium salt (1 wt.% Li⁶) dissolved in the blanket/shield water coolant could provide net tritium breeding ratios of 80% with reasonable coverage of the machine (4). This concept offers simplicity and conventional materials. At low temperatures (50°C) and pressures (0.2 MPa), corrosion is expected to be negligible with suitable salts. Tritium recovery by conventional techniques is feasible and economic at acceptable tritium levels (e.g., 10 Ci/L, based on the CANDU reactor moderator heavy water operation experience at comparable temperature and pressure).

The tritium breeding is improved with higher salt concentrations, neutron multiplier, zirconium alloy structure and heavy water. The concept may be extended to provide reasonable power reactor performance with blankets operating at conditions and with materials similar to those in present CANDU reactors. It may be especially attractive for very high power density devices such as reverse-field pinches.

FIGURE 3: FULL-COVERAGE TRITIUM BREEDING RATIO WITH 1 WT.% LI⁶ DISSOLVED IN H₂O

HELIUM-COOLED MOLTEN SALT BLANKET FOR FISSILE FUEL PRODUCTION

Fusion reactors to produce fission fuel take advantage of the existing electric utility investment in fission reactors, the higher neutron/power ratio of fusion, and the reduced requirements on the fusion reactor. A near-term fusion device could be economically attractive as a fission fuel producing device. The major disadvantage is the addition of fission reactor safety concerns to fusion.

Figure 4 shows a fission fuel production blanket (6). Fission is suppressed to minimize criticality concerns by using beryllium to multiply neutrons, rather than uranium, and by minimizing the fissile inventory. The fertile and fissile material are in a molten salt medium (LiF+BeF₂+ThF₄), which is constantly circulated outside the blanket and the U²³³ and tritium extracted. Helium cools the blanket, including the steel tubes containing the molten salt, by circulating through the beryllium pebble bed. Austenitic steel structure is used for ease of fabrication, adequate irradiation lifetime, and low corrosion rate by molten salts.

The molten fluoride salt is stable to both thermal and radiation decomposition because of the speed of recombination. Corrosion rates of iron-based alloys are low when the salt is maintained in a reducing state. This salt is similar to one used in an experimental fission reactor at Oak Ridge National Laboratory. It operates at relatively low pressure but high temperature - its melting point is 5300°C. Helium is used as coolant for safety and compatibility with the other materials. It is operated at higher than 5 MPa pressure in order to have adequate heat capacity.

FIGURE 4: HE LiUM-COOLED MOLTEN-SALT BLANKET FOR FISSILE FUEL PRODUCTION
The blanket has a small amount of lithium breeder present in the molten salt, but the large amount of multiplier allows sufficient tritium breeding (but little margin). Beryllium swells at modest fluences, so a pebble bed geometry is used to minimize stresses, add porosity, and simplify pebble replacement.

The tritium will be present in the molten salt as $^3$He gas, which will permeate readily through the hot steel walls. Tritium removal from the helium will be necessary because of this tritium and that generated in the beryllium. Permeation barriers will probably be needed for the steam generator, and possibly on the molten salt tubes.

Energy recovery from the helium can take advantage of direct gas turbine cycles, or by steam-generation, using a state-of-the-art technology. The $^{235}$U produced is estimated to be worth the equivalent of about $60/kg of U_3O_8. One 3000 MWth fusion device could supply 40 GWo of CANDE reactors at a capital cost of 2-3 times that of a 3000 MWth fission reactor.

**ADVANCED ENERGY CONVERSION BLANKET CONCEPTS**

One of the features of fusion is the very high quality energy available from the reaction in the form of energetic neutrons, short-wavelength radiation, and plasma kinetic energy. In some devices, escaping plasma kinetic energy can be directly recovered through direct electrical converters at efficiencies of 60% for simple grid systems.

In the blanket, most of the energy flux is in the form of neutrons and short-wavelength radiation that are not normally converted into electricity other than through Carnot-limited thermal power cycles. Recently, concepts have been proposed for recovering this energy as electricity at higher efficiencies (8).

One approach is the radiation-enhanced MHD blanket. Conventional non-fusion MHD electric power concepts based on coal, for example, suffered from several drawbacks that have limited commercial interest in the technology. In particular, the large magnets are expensive, and the hot gasses were not sufficiently conducting. In fusion, large and efficient superconducting magnets have been developed, built and tested. Such magnets are required to maintain the built-up fusion reaction in many confinement systems. Consequently, large volumes of high magnetic field are available in the blanket at small additional cost. A liquid metal vapor can be generated through the neutron thermal energy. Then, by appropriate design, radiolysis of the vapor species by incident synchrotron or X-ray radiation may make the vapor sufficiently conducting to support efficient electrical power generation. This could be arranged by passing the vapor through low Z structural regions near the first wall (for X-ray transmission) or by passing the incident synchrotron radiation through waveguides around the breeding blanket and then into the vapor. These concepts, although in a very early stage, could entirely eliminate the conventional heat transport and turbine-generator systems.

**SUMMARY**

There are many possible blanket concepts. The apparent wide range of options is important since blanket technology is in its infancy. Thus, the range of potential ideas makes it probably that at least once concept will prove commercially viable for each application. Indeed, there are many concepts that are probably feasible, and it is perhaps a question of determining which are the most attractive. Attention is turning to blanket development in anticipation of their use in next-generation devices. This initial data will help to screen out the concepts and focus the effort.

**REFERENCES**


SESSION E: THERMOHYDRAULICS I

Chairman: D.A. Meneley, University of New Brunswick

Experimental investigation of steam-line break transients in a recirculating U-tube steam generator.
G.R. McGee and V.S. Krishnan, AECL WNRE, and T.G. Bolander Ontario Hydro

STGEN simulations of top blowdown tests of RD-12 steam generator.
C.S. Kim, W.K. Liauw and J.Y. Stambolich, Ontario Hydro

Analysis of fuel element to pressure tube contact using the mini-smartt computer code.
D.B. Reeves, P.S. Kundurpi, G.H. Archinoff, A.P. Muzumdar and K.E. Locke, Ontario Hydro

Effect of gas flow in the insulating annulus on fuel channel temperatures in a severe accident in a CANDU reactor.
J.T. Rogers and S.S. Goindi, Carleton University

Thermal behaviour of CANDU fuel channel under steam flow conditions: An alternative solution.
H.E.S. Fath and K.M. Al-Sabti, University of Technology, Baghdad

D.J. Richards and T.E. MacDonald, AECL WNRE, and S.D. Grant, AECL CANDU Ops.
EXPERIMENTAL INVESTIGATION OF STEAM-LINE BREAK TRANSIENTS IN A RECIRCULATING U-TUBE STEAM GENERATOR

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ABSTRACT
An experimental investigation has been completed of the thermalhydraulic behaviour of a U-tube steam generator under conditions of simulated steam-line rupture. The object of this program was to provide data to verify steam generator models in computer codes used for reactor safety analysis. This paper describes the experiments and the results obtained, specifically, rate of inventory loss, steam generator voiding, liquid level response, and heat removal during the depressurization transient.

INTRODUCTION
In CANDU PHWR reactor licensing submissions, one of the postulated upset conditions to be considered involves a break in the steam-line piping of a steam generator, leading to a loss of normal heat sink, and possible loss of pressure control in the primary heat transport system.

A series of 22 experiments has been conducted at the Whiteshell Nuclear Research Establishment in which a medium-scale steam generator was depressurized through an orifice in the steam-line piping. The purpose of these experiments was to provide data to verify steam generator models in computer codes used for reactor safety analysis, and to provide information on the response of steam generator liquid level instrumentation and on the quantity of heat removed from the primary circuit during the depressurization phase. Data from these experiments are already in use in code development research [1, 2].

TEST FACILITY
Figure 1 is a schematic diagram of the test facility. The primary circuit consisted of an electrically energized heated section, a pump, and a steam generator, connected together in a closed loop. The reactor-typical steam generator (see Figure 2) was of the recirculating U-tube type, with internal preheater and downcomer. Four radial arms and a perforated plate inside the steam generator shell represented the steam-water separation system in a typical reactor steam generator. The steam generator was depressurized by opening a fast-acting valve in the steam-line. An orifice plate in the line determined the rate of depressurization.

Tube-side temperature, and shell-side pressure, temperature, void fraction, downcomer flow rate, and collapsed-liquid level were measured in the steam generator. The mass discharge rate at the break was measured using a full-flow turbine flow meter combined with a symmetric Venturi meter. The data were recorded using a PDP-11 computer.

TEST PROCEDURE AND CONDITIONS
To begin the test, primary circuit and steam generator shell pressures, heated section power, and steam generator liquid level were brought up to preselected steady-state operating conditions. The range of test conditions is given in Table 1.

At the start of each experiment, 10 s of steady-state data were recorded. The steam and feedwater valves (see Figure 1) were then closed to isolate the steam generator from the jet condenser and feedwater supply. The break valve was opened immediately thereafter.

In some experiments, because of the loss of normal heat sink, the primary pressure began to increase. If the pressure rose to a preset limit, power to the heated section automatically shut off. Usually, an experiment continued until the shell pressure reached atmospheric.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Break Size</td>
<td>0.1 - 5.0% of steam generator shell area</td>
</tr>
<tr>
<td>Primary Pressure</td>
<td>5 - 10 MPa</td>
</tr>
<tr>
<td>Shell Pressure</td>
<td>2.5 - 4.5 MPa</td>
</tr>
<tr>
<td>Heated Section Power</td>
<td>0.3 - 1.0 MW</td>
</tr>
<tr>
<td>Steam Generator Liquid Level</td>
<td>72 - 90% of shell height</td>
</tr>
</tbody>
</table>

GENERAL RESULTS

Effect of Break Size
The rates of depressurization and inventory loss depended primarily on the size of the break. For example (see Figure 3), for a test with a high input power in which the break size was large, the shell pressure fell to atmospheric and almost all of the inventory was discharged within about 20 s of opening the break valve. For a small break at the same
Figure 1: Schematic Diagram of Test Facility (T: Temperature, P: Pressure, Q: Differential Pressure and H: Level)
power, choking at the break orifice initially increased the shell pressure until the steam generation rate dropped due to depleted inventory. The shell pressure then decreased gradually to atmospheric.

![Figure 3: Effect of Break Size on Depressurization Transient](image)

**Effect of Input Power**

Input power also had a strong influence on depressurization rate (see Figure 4). In a test with a low input power the inventory loss was relatively slow, and about 900 s were required for the shell pressure to reach atmospheric. In a test with a high input power with the same break size, the inventory loss was much faster, and the shell pressure reached atmospheric in about 400 s.

![Figure 4: Effect of Input Power on Depressurization Transient](image)

**Effect of Initial Steam Generator Liquid Level**

The initial liquid level in the steam generator had a minor influence on the length of time required for
the shell pressure to reach atmospheric. The results of two tests with the same break size and input power are compared in Figure 5. Since the rate of inventory loss was primarily determined by break size and input power, the longer depressurization time for the test with a high initial inventory, as compared with the low initial inventory, was presumably due to the time required for the additional inventory to be lost.

![Figure 5: Effect of Initial Inventory on Depressurization Transient](image)

**FIGURE 5: EFFECT OF INITIAL INVENTORY ON DEPRESSURIZATION TRANSIENT**

**DETAILED TEST RESULTS**

The results obtained in a low-input power test and a high-input power test are described in detail below.

**Low Input Power Test Results**

The steam generator shell pressure began decreasing rapidly, immediately upon opening the break valve (see Figure 6). The depressurization slowed 40 s later, at about 1.4 MPa. During this rapid depressurization period, the discharge rate through the intermediate-sized break was high (see Figure 7) and the overall liquid level dropped rapidly (see Figure 8).

A period in which the conditions changed slowly followed the rapid depressurization. In this second phase, the pressure decreased slowly, and the discharge rate and overall liquid level were nearly constant. During this period, the boiling region around the tube bundle (see Figure 2) was voiding, and the tube side temperature drop was high (see Figure 9), indicating that the steam generator was a very effective heat sink.

About 120 s after the break, the tube bundle had almost emptied, and the shell pressure, overall liquid level, and tube-side temperature drop began to decrease. By about 250 s, the tube-side temperature drop had fallen to its minimum, and the liquid level, discharge rate, and shell pressure were all nearly zero. Data collection was stopped at 395 s, after the shell pressure had reached atmospheric, and increasing primary circuit temperature had indicated a complete loss of heat sink.

![Figure 6: Steam Generator Shell Pressure, Low Input Power Test](image)

**FIGURE 6: STEAM GENERATOR SHELL PRESSURE, LOW INPUT POWER TEST**

![Figure 7: Break Mass Discharge Rate, Low Input Power Test](image)

**FIGURE 7: BREAK MASS DISCHARGE RATE, LOW INPUT POWER TEST**
In this experiment, the break valve did not open at the same instant as the steam valve closed, so a small pressure spike occurred at the beginning of the transient (see Figure 10). After the break valve opened, the steam generator initially depressurized to about 4.3 MPa. The break size used in this experiment was one-fifth that used in the low-power experiment described above. By 30 s, a choking plane was established at the break orifice, which limited the discharge rate (see Figure 11). Since steam was being produced faster than it could be discharged through the break, the shell pressure began to rise. Steam separation and water recirculation continued with very little swelling, as may be seen by comparing the overall liquid level with the upper liquid level (Figure 12). By 160 s, the liquid inventory in the steam generator was nearly depleted, the tube-side temperature drop (see Figure 13) began to decrease (indicating decreasing effectiveness of the heat sink), the rate of steam generation began to decrease, and so the shell pressure began to decrease. The decreasing heat-removal rate resulted in a rise in the primary circuit temperature until boiling began in the heated section at about 200 s. At this point, the primary circuit pressure began to rise sharply, and the heated section power automatically shut off. The shell pressure continued to decay, and the experiment was stopped at about 420 s.

**FIGURE 8:** STEAM GENERATOR LEVELS, LOW INPUT POWER TEST (UNITS ARE m OF SATURATED LIQUID, ABOVE TUBESHEET)

**FIGURE 9:** STEAM GENERATOR TUBE-SIDE TEMPERATURE DROP, LOW INPUT POWER TEST

**CONCLUSIONS**

Experiments have been conducted to study the behaviour of a recirculating U-tube steam generator under conditions of a simulated steam-line rupture. The results described typify the results of experiments in the steam-line break program. The time-scale of events during the steam generator depressurization is, in general, shortened by larger break sizes, higher input powers and lower initial liquid levels, or, lengthened by smaller break sizes, lower input powers, and higher initial liquid levels.

The results have provided valuable information on steam generator thermalhydraulics during postulated upset conditions and have established a database against which computer models for predicting steam generator behaviour can be verified. Data from the experiments are already in use in code development work \[1, 2\].
FIGURE 11: BREAK MASS DISCHARGE RATE, HIGH INPUT POWER TEST

FIGURE 12: STEAM GENERATOR LEVELS, HIGH INPUT POWER TEST (UNITS ARE m OF SATURATED LIQUID, ABOVE TUBESHEET)

FIGURE 13: STEAM GENERATOR TUBE-SIDE TEMPERATURE DROP, HIGH INPUT POWER TEST

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REFERENCES


STGEN SIMULATIONS OF TOP BLOWDOWN TESTS OF RD-12 STEAM GENERATOR

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ABSTRACT

A computer code, called STGEN, has been developed for the purpose of analyzing the thermal hydraulic response of the steam generator to top blowdown under conditions representative of a postulated steam line break. This code was developed for best estimate predictions with a fast running capability.

Recently, a series of top blowdown tests was conducted using the RD-12 steam generator connected to the RD-14 experimental test loop at the Whiteshell Nuclear Research Establishment as part of the CANDEV program. Simulations of selected tests using the STGEN code have been performed. These results are presented for key thermal hydraulic parameters. Comparisons of these parameters are made to the measurements where available.

Good agreement is observed between the code prediction and the test data for the key parameters of interest. Such agreement provides confirmation of the adequacy of the model and assumptions employed in the code for the top blowdown simulations. This allows a high degree of confidence in the use of the code for accident analyses involving a break in the steam supply piping of a steam generator.

INTRODUCTION

Background

The steam generator provides the major heat sink and is one of the most important major components in CANUD nuclear power plants. Suitable heat removal through the steam generator is essential during normal or off-normal operating conditions. On the other hand, excessive or degraded heat removal capability of the steam generators caused by malfunction of supporting equipment or an accident involving the steam generators could lead to a loss of heat sink for the reactor.

Either as a controlling component or an accident initiating component, the steam generator is responsible for a wide range of transients in reactor operations. Therefore, a good steam generator model is important in order to understand the overall system behaviour and actual system response during normal or off-normal operating conditions.

Among the other general needs for accurate modelling of the steam generator behaviour, attention has been focussed on the blowdown of the steam generator. Such need arises as a result of the CANUD reactor licensing requirement for safety analysis, namely, a postulated break in the steam-line piping of a steam generator.

Due to the complexity of steam generator design in CANUD reactor plants, and the state of knowledge of top blowdown phenomena, however, a large number of assumptions and simplifications are employed in performing thermal hydraulic analysis of the postulated steam piping break. Unfortunately, these assumptions may be neither realistic in nature nor necessarily conservative.

Additionally, the steam generator top blowdown analyses in the past have demonstrated the sensitivity of the key thermal hydraulic parameters/variables to the thermal hydraulic model used in the simulations. The parameters/variables include the discharge flow and quality, the liquid inventory remaining in the steam generator, the water level transients and heat transfer rate during and following the blowdown. These parameters are essential to the determination of the availability of heat sink provision, reactor trip signals for effective reactor trip and system behaviour and response. These also have an impact on containment or powerhouse design, and equipment protection from harsh environment, all important to the safety of the power plant operation.

Therefore, experimental and analytical studies were undertaken to improve the understanding of the phenomena and resolve the modelling uncertainties so that a more realistic, perhaps less conservative, analysis can be performed with a high degree of confidence.

STGENerator Top Blowdown Computer Code

Considerable effort was expended at Ontario Hydro and the consulting firm of Fauske and Associates, Inc. retained by Ontario Hydro in improving modelling of the steam generator [1]. Information available from several small scale and large scale top blowdown experiments including the Marviken IV Test No. 11 was compiled and correlated in the form of an analytical model [2]. Further analytical and experimental work were undertaken to support the development of a computer program named STGEN.

This code focuses on many areas of top blowdown modelling which had not been considered in detail in the past: steam/water separation, water droplet carryover or liquid holdup, pool swell and propagation of mixture level, and heat transfer including dryout considerations. These phenomena are important to the top blowdown events and therefore modelled in detail. Although the code is developed as an independent, stand-alone computer program, it can be easily interfaced with the transient system computer code used in Ontario...
Hydro's licensing analysis, SOPHT [3]. The STGEN code is intended for best estimate predictions with the particular emphasis on a fast running capability.

RD-12 Steam Generator/RD-14 Test Loop

A number of small scale and large scale top blowdown experiments have been made available in the past which provided important information on many areas of top blowdown modelling for better understanding of the phenomena and eventual development of the analytical model for the STGEN code.

These tests were, however, lacking in that either the geometry was not representative of a steam generator, steam separating equipment was missing, no heat sources or the break to vessel area ratios were not large enough to cover all steam generator top blowdown events of interest. To overcome these experimental limitations, tests were committed in the RD-12 steam generator tied into the RD-14 loop as part of the CANDEV program.

This model steam generator has prototypic steam generator internal structures (steam/water separator and U-tubes, etc.), and the tests were conducted under representative operating conditions using the RD-14 loop with a single heated channel as the primary side circuit.

The test facility is shown in Figure 1. A schematic description of the RD-12 steam generator model is shown in Figure 2. A series of 22 top blowdown tests were conducted with break sizes ranging from 0.1% to 5% of vessel area and powers from 0.3 to 1.0 MW. The maximum power of 1.0 MW corresponds to full power operation in the prototype reactor. A more complete description of the test facility, test conditions and procedures are given in a separate paper on this subject by McGee, et al. in this symposium [4]. The tests provides an experimental data base for the STGEN code verification.

This paper introduces the main features of the STGEN code, and outlines briefly the STGEN model for the RD-12 steam generator top-blowdown experiments. The simulation results for two selected tests are presented along with the test data. The accuracy of the code predictions is demonstrated by comparison with the test data.

STGEN CODE

The STGEN model of the steam generator contains lumped parameter representations of those regions essential to a description of the internal hydrodynamic response. Typically, these regions are divided on the basis of the prevailing physical processes into:

1) the riser where active heat transfer across the U-tubes is taking place,

2) the steam drum where special attention is focused on the steam/water interphase and the physical modelling of the steam/water separators,

3) the downcomer region where the presence of liquid provides the major gravity head that affects the natural circulation flow through the tube bundle.
Provided is also made to extend the model to include a preheater region where the subcooled, incoming feedwater enters the steam generator. Within each region of the steam generator, the conservation equations are integrated to yield an overall regional representation of the thermal hydraulic variables.

The fundamental features of the analytical model incorporated in the code, the solution method, and the code structure are briefly described below.

Hydrodynamic Model

The basic hydrodynamic model is based on a one-dimensional, two-equation formulation for two phase flow. It consists of one mixture mass equation and one mixture energy equation. The momentum equation is not a primary governing equation because the detailed pressure distribution is not important during the blowdown process. Instead, the average pressure of each region is calculated as a state variable. Once the average pressure is determined, the distribution of pressure within the region is calculated by taking into account the influence of gravity, friction and acceleration pressure loss effects.

The transient two phase flow is assumed to be in thermal equilibrium but not homogeneous. The difference between liquid phase velocity and vapour phase velocity is taken into account by a flow regime dependent drift flux model. Three flow regimes are considered: the bubbling flow regime, the churn turbulent flow regime, and the (liquid dispersed) droplet flow regime. Bubbling flow exists only when the void fraction is low and therefore can exist in the downcomer and the lower part of the steam generator during the early part of blowdown. During blowdown, the flow is expected to be highly turbulent and hence in the churn-turbulent flow regime. The droplet flow regime is likely to occur in the large void fraction region at the upper portion of the riser section and the steam dome above the separator. The criterion for flow regime determination is not built in to the code, for maximum flexibility, the selection is left to the user's specification.

The vapour velocity relative to the liquid, namely the drift flux, is a function of flow regime, gravity heads, the flow rate and the stagnation pressure itself. This relative velocity in turn influences the value of the interfacial and external mass and energy flow rates. In the STEN code, the void at the top, average void and the void at the bottom of each region are analytically derived, depending upon the flow regime which a region undergoes at any given time during the transient.

Water Level Model

Two types of water levels are considered, namely, the collapsed water levels and the mixture (swell) water level. The swell level represents the actual liquid distribution in the region, while the collapsed level represents the liquid mass contained within the region. If the void below the swell level is removed, the swell level becomes the collapsed level.

In STEN, a model of water level propagation is included which is based on a volume balance for the steam space in a region above the swell height. The rate of change of the swell height is modelled as a balance between the top vapour source and the superficial vapour escape rate across the vapour and water interface at the swell level. The superficial vapour escape rate is based on a model proposed by Zuber [5] and is a function of flow regime.

Heat Transfer Model

During the top blowdown, the secondary side pressure drops and the temperature difference across the tubes increases. Therefore, nucleate boiling heat transfer occurs which may result in dryout of the tube outer surfaces. The region of dryout marks the transition between high heat transfer rate and low post-dryout heat transfer rate.

In the STEN code, the tube bundle heat transfer model includes the whole range of heat transfer regimes from pre-dryout to post-dryout through the appropriate dryout criteria. This model also has a feature tracking the location of dryout in calculating the heat transfer rate by a transient interaction method using a set of dryout criteria.

To account for the heat capacity of the metal structure, including the tube wall, the tube wall temperature profile is calculated and both its inner and outer surface temperatures across the tube wall are determined. The structures are modelled by a one-dimensional heat conduction model.

Using the wall surface temperature, the appropriate heat transfer coefficients, and the dryout location, it is possible to calculate the overall heat transfer coefficients.

Additional Components Models:

The STEN code also includes component models required for the steam generator blowdown simulation such as:

1) A simplified primary system model to calculate the average primary system temperature for estimating the heat transfer rate across the tube bundle. The energy equation for the primary fluid in the steam generator is solved with a given core heat generation rate.

2) A level instrumentation response model to determine the level transducer response through a complex tracking of the level swell and void distribution over a pair of the measuring legs for a transducer.

3) A separator model to determine the separator flow, quality, degree of mixing and separation, etc.

Each component model is programmed separately for each of maintenance and separate verification.
Method of Solution

The solution method employed is a two-step scheme. Firstly, the energy and mass equations of each region are solved for the value of the inter-region and external mass and energy flow rates. The energy equation includes the internal energy sources. Secondly, the regional average pressure and its distribution within the region, the void distribution, heat transfer and the rest of the component models are solved using the previously calculated mass and energy flow rates. Internal iterations are required for some inter-region mass flow calculations where they are functions of the pressures across the regions. This procedure is repeated, while marching in time.

In order to ensure numerical stability, a first or third order Runge-Kutta integration scheme with an error check is used to solve the conservation equations.

Time Step Control

The size of the time step is selected by either using a constant value (user input) or a variable time step. The variable time step is determined within the user specified range by successively increasing the time step (over the previous time step) by a given fraction to maintain stable integration of the dynamic equations in a transient iteration.

The Code Structure

The STGEN code is of modular construction and structured with the following four levels of execution hierarchy:

1) the main program
2) initiation
3) transient routines
   - region models
   - library/utility subprograms
4) the output routines

A schematic diagram of the program is shown in Figure 3 along with a brief description of the main functions executed at each level.

STGEN MODEL FOR RD-12 STEAM GENERATOR TOP BLOWDOWN TESTS

The STGEN model for the simplified three-region RD-12 steam generator and RD-14 test loop is shown in Figure 4. The steam generator and the test loop are modelled by four separate regions: the riser, the steam drum, the downcomer, and the primary system.

The riser region includes the tube bundle and the separator neck; the steam drum extends from the upper plate of the riser section to the steam nozzle at the top; the downcomer is the narrow annular region bounded by the shell ID and the...
The shroud OD; and the primary system consists of the coolant in U-tubes, the cold and hot legs, and the core containing the heater element.

The riser region contains the U-tubes and the bundle exit quality is derived from the measured downcomer flow and steam flow rates prior to the break initiation.

The steam drum model consists of two volumes having equal pressure but different void distribution and swell sources. The boundary between the two volumes is at the top of the separators. A schematic diagram of the steam drum is shown in Figure 5. Prior to the break initiation, all the liquid inventory is in the lower volume. When the swell level exceeds the top of the separators, however, liquid enters the upper volume. Two volume representation of the steam drum enhances the code capability of detailed level swell prediction.

The downcomer region occupies a relatively small fraction of the total secondary side free volume. Since the lower legs of the level measurement transducers are tapped into this region, an accurate geometrical representation of the downcomer is necessary.

The space averaged primary coolant temperature in the steam generator is obtained from the energy equation for the primary fluid by considering the overall heat transfer rate from the primary fluid and the heat generation rate in the core.

Over a hundred input variables to the STGEN program are required for each simulation. Of these, a set of selected input data pertaining to geometry is shown as an example in Table 1 complete with explanatory notes.

**SIMULATION RESULTS AND DISCUSSION**

The STGEN code simulates the transient responses for the following important variables to the top blowdown:

- pressure (temperatures) in the various regions,
- total mass inventory and its distribution among the various regions,
- void fraction and quality distribution (average, top and bottom voids or qualities of each region),
- inter-region and external (break or feed) flow rates,
- liquid levels (both collapsed and swell) in the regions,
- level instrumentation response,
- primary coolant temperature and the resulting tube bundle power,
- overall heat transfer rate to the secondary side system, and
- location of dryout in the tube bundle.

Comparison of the model predictions with the experimental data of RD-12 steam generator top blowdown tests has been made for a small and a large break. The test geometry data are summarized in Table 2 for these two tests. Other tests were also simulated using STGEN; however, in this paper, discussion will be limited to these two tests.

Only a limited number of variables were measured in the tests. Therefore, the number of variables that can be directly compared with the predictions are also limited. Some additional predicted variables are presented here for a better understanding of the predictions and interpretation of the test results.

Ten seconds of steady state data were logged before the break valve was actuated to open. Simulations were performed including the time period of ten seconds prior to the break in order to assess the steady state level established prior to the transient simulations.

**TABLE 1: STGEN Input Data for RD-12 Top Blowdown Simulation**

<table>
<thead>
<tr>
<th>Variable</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>V(1)</td>
<td>4.8130</td>
</tr>
<tr>
<td>H(1)</td>
<td>2.9250</td>
</tr>
<tr>
<td>V(2)</td>
<td>4.8130</td>
</tr>
<tr>
<td>H(2)</td>
<td>2.9250</td>
</tr>
<tr>
<td>V(3)</td>
<td>2.7800</td>
</tr>
<tr>
<td>H(3)</td>
<td>2.1000</td>
</tr>
<tr>
<td>V(4)</td>
<td>2.7800</td>
</tr>
<tr>
<td>H(4)</td>
<td>2.1000</td>
</tr>
</tbody>
</table>

The downcomer region occupies a relatively small fraction of the total secondary side free volume. Since the lower legs of the level measurement transducers are tapped into this region, an accurate geometrical representation of the downcomer is necessary.

The steam drum model consists of two volumes having equal pressure but different void distribution and swell sources. The boundary between the two volumes is at the top of the separators. A schematic diagram of the steam drum is shown in Figure 5. Prior to the break initiation, all the liquid inventory is in the lower volume. When the swell level exceeds the top of the separators, however, liquid enters the upper volume. Two volume representation of the steam drum enhances the code capability of detailed level swell prediction.
### Table 2

**RD-12 Top Blowdown Test**
Steam Generator Geometry and Test Conditions

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Overall Height of SG Vessel (m)</td>
<td>5.0</td>
</tr>
<tr>
<td>Rated Primary Side Flow (kg/s)</td>
<td>6.0</td>
</tr>
<tr>
<td>Heat Transfer Area (m²)</td>
<td>7.82</td>
</tr>
<tr>
<td>Number of Tube</td>
<td>42</td>
</tr>
<tr>
<td>Tube I.D. (mm)</td>
<td>12.7</td>
</tr>
<tr>
<td>Overall Tube Length (m)</td>
<td>4.77</td>
</tr>
<tr>
<td>Steam Drum Free Flow Area (m²)</td>
<td>6.29-2</td>
</tr>
</tbody>
</table>

### Small Break Simulation and Comparison

Simulation of a small break has been done for the time period of 60 seconds. By this time the rapid blowdown is over and slow depressurization to atmospheric is taking place. Comparison of the predictions with the test data are shown in Figures 6 to 14. This break represents about 0.5 percent in break to vessel area ratio for the vessel I.D. of 292 mm.

Immediately following the break valve actuation (open) at 10 s, a rapid depressurization of the steam drum takes place as shown in Figure 6. A large increase in the discharge flow rate is both predicted and measured (Figure 7). Pool swell is sufficient to reach the top of the steam generator (Figure 8), causing transition from single phase steam to two phase discharge (Figure 9). In about 30 s, swell level decreases, uncovering once again the steam nozzle and leading to transition back to single phase steam discharge (Figure 8). The void fraction measurement (and also prediction) in Figure 9 also accords with this observation.
The measured depressurization rate is shown to be higher than that predicted by the code until approximately 30 s. During this period, the discharge is two phase. The discharge flow rate as shown in Figure 7 is seen to be slightly underpredicted by the code. At approximately 30 s, the predicted discharge flow rate overshoots that of the measurement and the predicted depressurization rate increases over that measured. The depressurization appears to be sensitive to the discharge flow rate, particularly when in two phase. In general, the trends predicted by the code are correct and quantitative agreement is also good. However, some differences are noted between the predictions and the measurements: these differences are either small or minor in nature and more importantly can be accounted for.

Figures 10 through 12 show the level instrumentation responses. The test results show level oscillation immediately following the break. This oscillation lasts for about 1 to 2 seconds. Such oscillation has been observed in many top blowdown experiments including that of Marviken Test [2]. This behaviour is thought to be caused by the depressurization wave propagation associated with break initiation not modelled in the code.

The response of the narrow range measurement within the steam drum is shown in Figure 10. This instrument layout is representative of that used in the actual steam generator design for plant control and reactor trips. Good agreement is apparent.

The downcomer level measurement is now well represented (Figure 11). Although the difference in the downcomer level transient between the prediction and measurement appears to be large, the amount of liquid mass in the downcomer responsible for this difference is rather small. This is because the very narrow annulus shape and small volume of the downcomer makes the level response in this region highly sensitive to the difference in the amount of liquid mass. Due to its small volume, the downcomer does not contribute significantly to the overall blowdown phenomena, and the level measurement in this region is of limited importance only to that localized region.

The agreement in the wide range level response is better than that observed for the downcomer level response (Figure 12). With the exception of the initial oscillations in all of level measurements, good agreement is demonstrated in the level responses.

In Figure 13, the predicted temperature at the steam generator primary coolant exit plenum is compared with experiment. The temperature measurement at this location is a good indication of the overall heat removal rate by the steam generator during the transient. Good agreement is shown in the trend, and the rate of temperature decrease until about 30 seconds. Beyond this time, faster cooldown of the primary side coolant than measured is predicted. The discharge flow prediction being larger than the measurement beyond 30 s (Figure 7) is also responsible for this difference in cooldown rate.

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The depressurization transient is shown in Figure 15. In 50 seconds, the steam generator is shown to be essentially depressurized to atmospheric. The agreement between the code prediction and measurement is excellent. The discharge void fraction, i.e., amount of water carryover in the discharge flow, is compared in Figure 16. Again, excellent agreement is demonstrated. This indicates that pool swell and the void distribution predictions in the steam drum are also accurate. The predicted swell and collapsed levels, namely the results of pool swell and void distribution estimates in the steam drum, are shown in Figure 17.

Figure 14 shows the prediction of liquid mass depletion transient. Immediately following the break, a sudden surge of flow from the riser to the steam drum occurs. As a result, the liquid mass in the riser section is rapidly depleted, while the steam drum mass increases momentarily. At the end of the simulation, approximately 60 percent of the initial liquid inventory is discharged from the steam generator. Of 40 percent liquid inventory remaining, about one half is in the riser section, providing continuous heat sink for the primary coolant. A comparison could not be made of these parameters since they were not measured in the experiment.

Large Break Simulation and Comparison

Simulations of a large break have been performed for 50 seconds. Comparisons of the simulation results with the test are shown in Figures 15 to 23. This break represents a break to vessel area ratio of about 2 percent. Most large steam line breaks in CANDU stations are in this range of break to vessel area ratio.

Figure 15: Pressure Transients Comparison

Figure 16: Steam Line Void Fraction Comparison
In this test, no comparison with discharge flow is possible since the spool piece for break discharge flow measurement was not installed. Good agreement in both depressurization rate and discharge void fraction, however, indicates that the discharge flow is also well predicted.

Due to the large discharge, the mass inventory in the steam generator is depleted relatively quickly in comparison with the previous case. Figure 18 shows the mass depletion history. Towards the end of blowdown at 50 seconds, only about 15 percent of mass inventory is shown to remain in the steam generator.

Liquid drain from the steam drum, occurring earlier in the prediction than in the test has an effect on the level response predictions for both the downcomer and wide range level measurements (Figures 20 and 21): the level predictions are higher than the measurements beyond 18 seconds. This indicates that more liquid mass inventory exists in the downcomer than that measured. As discussed in the small break simulation above, the relatively small amount of liquid mass in the downcomer is responsible and therefore this has little significance on the overall blowdown phenomena.

The primary coolant temperature comparison at the steam generator exit plenum is shown in Figure 22. The temperature rise towards the end of blowdown is also captured well by the code prediction; however, the timing when the rise starts is slightly earlier in the simulation. Dryout criteria might have been applied earlier in the code than actually occurred in the test.
Many temperature measurements were provided at various locations along the tube bundle from the entrance on the hot leg side to the exit at the cold leg side of the tube bundle. Using these measurements, it is possible to ascertain the propagation of dryout location along the tube. This location constructed in this way is compared with the code predicted dryout located in Figure 23. Excellent agreement is seen in this comparison.

The comparisons of the primary side temperature at the steam generator exit and the dryout propagation both indicate that heat removal by the steam generator during the large break blowdown is well represented by the code.

In this regard, it is worth mentioning that heat removed by the steam generator is both experimentally and analytically demonstrated to be quite effective throughout the blowdown, even with the liquid mass being significantly depleted. This is an observation of importance to plant safety analysis since the heat transport system is then depressurized as well.

CONCLUSION

The STGEN computer code has been developed for simulation of steam generator top blowdown events at Ontario Hydro with assistance from Fauske and Associates, Inc. under contract to Ontario Hydro. The STGEN code provides predictions of the thermal hydraulic variables and parameters important to plant safety analysis involving the postulated steam line break.

The results presented in this paper have demonstrated the capability of the STGEN model in predicting the behaviour of the model RD-12 steam generator in the top blowdown event. A large number of measurements have been made available for code verification through a series of tests using the RD-12 steam generator with prototypic internal structures tied to the RD-14 test loop to provide the conditions representative of those in actual plants.

STGEN correctly predicts the major qualitative trends of the tests and also predicts many of the quantitative features. The additional STGEN advantages are the ease and accuracy with which steady-state initial conditions can be calculated, and the rapid running speed. STGEN ran approximately in real time without any optimization of the run times using UNIVAC 1108/82Y.

The overall good agreement between the STGEN calculations and measured data provides confidence in the capability of the STGEN program to simulate the response of the real plant to a top blowdown event.

ACKNOWLEDGEMENTS

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The authors also would like to express their gratitude to W.I. Midvildy and R.E. Pauls for their comments and suggestion on this paper.
REFERENCES


ABSTRACT

This paper quantifies the thermal-mechanical effects of potential contact between the fuel element and the pressure tube (FE/PT contact) during a postulated large break LOCA transient in a CANDU reactor. At issue is the possibility of local strain failure of the pressure tube early in the LOCA transient. Calculations have been performed using the code MINI-SMARTT which was developed specifically for this analysis. It is shown that pressure tube local strain is most sensitive to the contact conductance, contact width, and transient time of FE/PT contact. The strain response of the pressure tube is derived over a broad range for the above parameters. A critical FE/PT contact conductance value is derived as a function of contact width. Assuming Hertzian mechanics, the critical contact conductance is sufficiently high that pressure tube failure would be precluded.

INTRODUCTION

The CANDU reactor employs a large number of horizontal fuel channel assemblies, each surrounded by heavy water moderator within the calandria vessel. The fuel channel assembly provides the pressure boundary between the high pressure heat transport system and the low pressure moderator system. Each fuel channel consists of a Zr-2.5% Nb pressure tube contained within a Zircaloy calandria tube which is attached to the calandria vessel tubesheet and separates the pressure tube from the moderator. The fuel bundle consists of short (about 0.5 m), closely spaced fuel elements welded to Zircaloy end plates (Figure 1). The fuel sheath is made of thin wall (0.42 mm) Zircaloy. The string of bundles in the fuel channel rests on the pressure tube, separated from it by bearing pads on the outer elements of the fuel bundle.

During the postulated large-break Loss-of-Coolant Accident (LOCA), rapid voiding of the coolant may occur in some fuel channels, followed by a period of steam cooling. During this degraded cooling period, changes in geometry of the fuel and fuel channel components will occur if sufficiently high temperatures are reached. Among the possible distortion modes, diametral straining (ballooning) of the pressure tube into contact with the calandria tube occurs if the pressure tube temperature and internal pressure are high. As well, the fuel elements could distort into contact with the pressure tube (FE/PT contact) if sufficiently high temperatures are achieved (See Figure 2). If FE/PT contact occurs prior to pressure tube ballooning it may create a hot spot on the pressure tube leading to local strain failure. Rupture of the pressure tube may be precluded if the overall heat flux to the pressure tube causes rapid ballooning. The resultant rejection of heat to the cool moderator prevents further straining of the pressure tube.

This paper presents the results of analytical studies, using the MINI-SMARTT computer code, which predict the transient thermal-mechanical behaviour of the pressure tube following the contact of a hot fuel element under LOCA conditions. Specifically, the paper addresses the conditions under which rupture of the pressure tube due to local wall thinning, prior to pressure tube/calandria tube contact, may occur. An assessment of the margin to pressure tube failure under LOCA conditions is included assuming Hertzian mechanism to predict the width of FE/PT contact.
DEVELOPMENT OF THE MINI-SMARTT COMPUTER CODE

The FE/PT contact code MINI-SMARTT was developed from the existing computer code SMARTT (Simulation Method for Axial and Radial Temperature Transients). SMARTT was originally developed to predict two-dimensional pressure tube circumferential temperature distributions under asymmetric stratified coolant and geometry conditions.

SMARTT employs a finite difference technique to solve the heat conduction equations for radiation, convection from the coolant, and conduction through the fuel elements and pressure tube. The equations are solved implicitly using a sparse matrix solver. Allowance is made for temperature dependent properties based on current temperatures through an iteration scheme.

Modelling Features of the MINI-SMARTT Code

To assess FE/PT contact, the SMARTT code was modified to include the effects of contact conduction between any specified number of fuel elements and the pressure tube. The resultant two-dimensional MINI-SMARTT model is illustrated in Figure 3 for single fuel pin contact. The pressure tube and fuel elements may have any number of radial and azimuthal nodes, graded from fine to coarse proceeding away from the FE/PT contact region. The temperatures associated with non-contacting fuel pins (ring model) and the transient thermal-hydraulic boundary conditions of fuel power, coolant pressure, temperature, and convective heat transfer coefficient to the fuel and pressure tube are supplied separately. The heat generation modes considered are fission and decay power with bundle and element flux depression and metal-water reaction on the outer surface of the fuel sheath and inner surface of the pressure tube.

MINI-SMARTT Heat Transfer Models.

In developing the MINI-SMARTT code, simplified heat transfer models were employed so that the parameters affecting FE/PT contact could be studied for their sensitivity on the prediction of pressure tube rupture. As well, the CANDU licensing philosophy of using a conservative approach when uncertainties exist, was followed.

The modes of heat transfer modelled in MINI-SMARTT are summarized in Figure 4. These include:

- radial and azimuthal conduction within each fuel element specified in FE/PT contact;
- convection from sheath-to-coolant and coolant-to-pressure tube;
- ring radiation from the outer fuel ring to the pressure tube;
- radial and circumferential conduction within the pressure tube;
- contact conduction between specified fuel elements over any contact width.
The detailed pin radiation model in the 37-fuel element version of the SMARTT code gives a 186 by 186 view factor matrix with about 2,000 non-zero entries. Since this view factor matrix is applicable only to a specific fixed geometry, the use of a pin radiation model for the entire fuel bundle was not practical to study the transient effects of FE/PT contact. Any alteration of the number of contacting fuel pins, length of contacting arc between fuel and pressure tube, and graded nodalization scheme would invalidate the view factor matrix. Thus, in order to retain the required analytical flexibility, MINI-SMARTT uses the simplified, conservative ring radiation model illustrated in Figure 4. This approach produces conservatively high radiation heat fluxes to the region of the pressure tube near the contact point. The heat flux is overestimated because of the use of the ring temperature, rather than the temperature of the lower half of the fuel pin in contact with the pressure tube. The radiation heat flux emitted from the contacting fuel pin(s) is simulated in the code by applying input view factors specific to the nodalization scheme of the contacting pin(s). The conduction heat transfer between the pressure tube and the contacting fuel sheath is calculated using an input value of contact conductance for each individual node in contact. In the calculation of convection by the coolant, transient heat transfer coefficients for the fuel and pressure tube are input from the thermal-hydraulic conditions.

Calculating Pressure Tube Strain

In order to calculate the transverse creep strain associated with the non-uniform ballooning which may occur due to FE/PT contact, a separately developed strain code NUBALL (Non-Uniform-Ballooning) has been added to MINI-SMARTT as a subroutine. The equations for transverse strain used in the code have been verified against uniaxial pressure tube strain experiments.

The code uses the geometric properties of the pressure tube, the internal pressure, and the temperature distribution around the pressure tube circumference to determine strain. Both average pressure tube strain and local strain in each node (wall thinning) are calculated. The local strain at any node is continuously compared with a derived pressure tube failure criterion, while the average strain is compared to the strain required for pressure tube/calandria tube contact (about 6.4%). The status of these conditions is continually monitored and reported in the transient output.

MINI-SMARTT Nodalization and Time Step Sensitivity

Extensive nodalization studies were conducted prior to using MINI-SMARTT for FE/PT accident analysis. The nodalization studies were performed in order to optimize the spatial convergence of the graded node grid in both the fuel elements and the pressure tube. Figure 3 gives the recommended nodalization grid determined from these studies.

Time step studies were conducted using calculation times of 0.05s, 0.1s, 0.2s, and 0.5s. Bulk radiation heat transfer from the fuel bundle was neglected in order to demonstrate the effects of contact conduction alone. The resultant pressure tube circumferential temperature profiles for the inside surface of the pressure tube are shown in Figure 5.

These results indicate that a calculation time step of the order of 0.1s gives accurate pressure tube temperature profile predictions since smaller time steps do not significantly alter the results. Thus, the 0.1s time step was used in the following analysis.

Analytical Methodology

The thermal-hydraulic boundary condition used in this study were for a large break LOCA scenario which provided the maximum fuel heat-up rate and thus the greatest potential for FE/PT contact. The analysis was conducted for 28-element (Pickering-type) fuel bundles.

The reference case conditions are given in Table 1 as well as the parameters studied for their sensitivity on the model predictions. The methodology considered the potential for single fuel element or multi-fuel element contact. Parametric studies were conducted for the single element contact case. The effect of multi-element contact was then determined for the limiting case derived from the single element parametric studies. The parametric studies considered were FE/PT contact conduction ($h_{con}$), fuel to fuel sheath heat transfer coefficient, initial fuel power, contact width, timing of contact, and the effect of the neutronic power transient.

Results of Single-Element Contact Studies

During the initial studies for single element contact, it was found that the model predictions were not sensitive to the channel power, neutronic power, and the fuel to fuel sheath heat transfer coefficient. For the sake of brevity, these results will not be presented in detail. The following sections discuss the model predictions
**TABLE 1**

FE/PT Contact Study - Reference Case and Sensitivity Conditions

<table>
<thead>
<tr>
<th>Break:</th>
<th>Reference</th>
<th>Sensitivity</th>
</tr>
</thead>
<tbody>
<tr>
<td>Break:</td>
<td>40 percent LOCA</td>
<td></td>
</tr>
<tr>
<td>Channel Power:</td>
<td>6.1 MW channel with highest fuel temperature</td>
<td>5.1 MW</td>
</tr>
<tr>
<td>Neutronic Power:</td>
<td>hottest bundle in the core</td>
<td>average bundle transient</td>
</tr>
<tr>
<td>Pressure Tube</td>
<td>radial - 10 evenly spaced nodes</td>
<td></td>
</tr>
<tr>
<td>Nodalization:</td>
<td>azimuthal - 58 graded nodes</td>
<td></td>
</tr>
<tr>
<td>Fuel Element</td>
<td>radial - 18 graded nodes</td>
<td></td>
</tr>
<tr>
<td>Nodalization:</td>
<td>azimuthal - 26 evenly spaced nodes</td>
<td></td>
</tr>
<tr>
<td>Number of Contacting Elements:</td>
<td>single pin</td>
<td>three pins</td>
</tr>
<tr>
<td>FE/PT Contact Time:</td>
<td>10 seconds</td>
<td>Range: 0 seconds to just prior to ballooning</td>
</tr>
<tr>
<td>Nominal Fuel to Fuel Sheath Heat Transfer Coefficient:</td>
<td>10. kW/m²°C</td>
<td>5, 25 kW/m²°C</td>
</tr>
<tr>
<td>FE/PT Contact Width:</td>
<td>2 mm</td>
<td>0.1 to 4 mm</td>
</tr>
<tr>
<td>FE/PT Contact Conductance:</td>
<td>treated parametrically</td>
<td>0.5 to 20.0 kW/m²°C</td>
</tr>
</tbody>
</table>

for some of the more sensitive parameters investigated.

**Fuel Element/Pressure Tube Contact Conductance.**

The value of \( h_{\text{con}} \) between a contacting fuel element and the pressure tube is a function of temperature, applied pressure, and the surface material properties. There are presently few experimental data directly relevant to FE/PT contact conductance. However, experimental programs at the Whiteshell Nuclear Research Establishment (WNRE) are currently underway to determine \( h_{\text{con}} \), as well as the contact width, as functions of the above variables. Further insight into likely values of \( h_{\text{con}} \) is obtained from results of experiments on pressure tube ballooning, and combined sagging/ballooning. These experiments show that following initial contact, the contact conductance between the pressure tube and calandria tube is between 1.0 and 2.5 kW/m²°C, even when the internal pressure is as high as 2 MPa. Lower values of contact conductance are expected for FE/PT contact, particularly during the short period of time between contact and pressure tube ballooning.

The effect on the pressure tube temperature profile of \( h_{\text{con}} \) – 2.0 and 3.0 kW/m²°C is shown in Figure 6. Contact is assumed to occur at four seconds following the break in both cases. As expected, the higher value of \( h_{\text{con}} \) elevates the pressure tube temperature locally, but does not affect temperatures in the bulk of the tube.

**Effect of FE/PT Contact Time.**

To provide an understanding of the importance of the transient contact time (\( C_T \)), the sensitivity of the results to FE/PT contact occurring at times ranging from zero seconds to just prior to ballooning (14 seconds) are assessed for the reference case conditions.

**FIGURE 6**

**PRESSURE TUBE CIRCUMFERENTIAL TEMPERATURE PROFILE FOLLOWING FE/PT CONTACT**

Contact Time (\( C_T \) = 4s)

Figure 7 shows the effect of varying both \( C_T \) and \( h_{\text{con}} \) on the ultimate average pressure tube strain. Any combination of \( C_T \) and \( h_{\text{con}} \) which gives values of average strain equal to 16.4 percent results in pressure tube/calandria tube contact without causing pressure tube rupture due to local strain. This figure shows that the later the FE/PT contact time, and the lower the value of \( h_{\text{con}} \), the greater the likelihood of pressure tube/calandria tube contact without pressure tube failure. Very early contact times are not realistic since fuel deformation would not occur when the fuel is at the relatively cool temperatures associated with early transient times.
Effect of Contact Width.

The mechanical behaviour of the pressure tube is directly affected by the width of contact between the fuel element and the pressure tube. The width of contact is a function of the forces acting on the contacting fuel element, the temperature of the fuel element, and the surface roughness of the fuel element/pressure tube interface.

This study treats contact width parametrically to determine the sensitivity of the pressure tube mechanical behaviour to this parameter. The previous sensitivity studies discussed above used a total contact width of 2 mm, which corresponds to a contact angle of 15° on the fuel element. This is judged to be a conservatively large value, particularly for the time period immediately following contact. The sensitivity analyses are carried out using contact widths ranging from 0.1 mm to 4 mm.

Figure 8 presents the average pressure tube strain versus $h_{\text{con}}$ for contact widths of 0.1 mm, 1 mm, 2 mm, and 4 mm for the reference case contact time of 10 s. Thus, the 2 mm curve in Figure 8 corresponds to the $C_\text{PF} = 10$ s curve in Figure 7. It is apparent from Figure 8 that reductions in contact width significantly increase the value of $h_{\text{con}}$ at which pressure tube rupture is precluded. For example, with a contact width of 2 mm the critical contact conductance is 3.75 kW/m²°C, whereas for a more realistic contact width of 0.1 mm a contact conductance as much as 15.0 kW/m² can be tolerated.

Effect of Multi-Pin Contact

This sensitivity study is carried out to show that single-pin contact is more limiting than multi-pin contact. For reasons of symmetry in the modelling, contact of three pins is simulated rather than two-pin contact. Two cases of three-pin contact are examined, the first case being the reference case reported in Table 1 with a contact conductance of 4.0 kW/m²°C. The second case differs from the first in that the F/R:PT contact time is set to 0 s. Figure 9 shows the resultant pressure tube temperature profile for three-pin contact occurring at 0 s. It can be seen that the pressure tube temperature between the contacting pins is slightly higher than the temperatures well away from the contact region.

Figure 10 compares the strain results of the three-pin contact cases with those for single-pin contact. The figures shows that the average strain at pressure tube rupture for the case of 0 s contact is 8 percent, while single-pin contact gives 7 percent local strain. As well, for the case with F/R:PT contact set to 10 s, pressure tube/calandria tube contact occurs with $h_{\text{con}} = 4.0$ kW/m²°C, whereas for the single-pin contact case, the critical value of $h_{\text{con}}$ is 3.75 kW/m²°C. Thus, over the range of conditions considered in Figure 10, multi-pin contact produces greater average strains and leads to higher critical values of contact conductance than single-pin contact. Single-pin contact is, therefore, the most severe case for analysis of F/R:PT contact.
This section summarizes analysis which examined the various possible mechanisms for FE/PT contact and determined the minimum fuel sheath temperature for contact and the contact width using Hertz's theory.

The various mechanisms which could cause lateral deflection (bowing) of a fuel pin are:

(a) Bending of a fuel pin due to circumferential variation of temperature. However, as the fuel pin is generally hotter towards the centre of the fuel bundles in a large break LOCA, the resulting bending is expected to be towards the hot side when the sheath is assumed isotropic. Hence this mechanism is not considered in the present analysis.

(b) Sagging of a fuel pin due to the weight of pellets and sag magnification due to end compressive loads. The load required for sag magnification leading to FE/PT contact is very close to the buckling load. Hence sag magnification due to end compressive loads need not be considered as an independent mechanism.

(c) The only significant mechanism that can cause FE/PT contact is the buckling of a fuel pin due to restraint of the thermal expansion. By comparing the maximum axial restraint forces that can be developed for friction with the axial force required for buckling of a single fuel pin. It is concluded that fuel pin temperatures in excess of 1200°C are required for FE/PT contact.

For the LOCA conditions used in this study and the 6.1 MW channel power, the fuel pin does not reach 1200°C until approximately 4 seconds. Thus, contact times earlier than 4 seconds are not realistic since no mechanism can exert sufficient loads to cause fuel pin bowing.

Area of Contact Between Fuel Pin and Pressure Tube

When the mean temperature increase in a fuel pin exceeds the critical temperature increase (at a fuel sheath temperature of about 1200°C) it will result in elastic buckling of the fuel pin. An estimate of the maximum deflection can be obtained by assuming that the neutral axis of the fuel pin will bend into a circular arc to accommodate the strain (as in the case of bending of beams). The resultant bow displacements are very large compared with the elastic yield displacement of the fuel pin.

Hence, it can be reasonably assumed that a uniform reaction force develops over the contact area due to bowing contact of a fuel pin. An estimate of the uniform lateral load was found to be 130 N/m by considering that the contact pressure is limited to the uniform load required for yielding of the fuel pin in lateral bending.

Knowing the pressure acting on the contacting surfaces, the width of contact can be obtained by assuming a perfect elastic behaviour at the point of contact for both pressure tube and fuel pin. Based on Hertz's theory of contact for cylindrical bodies, the width of contact was found to be 0.015 mm for a fuel pin at 1000°C contacting a pressure tube at 300°C. This is a factor of 10 smaller than the minimum contact width shown in Figure 8.

CONCLUSIONS

The results presented above quantify the conditions under which FE/PT contact could lead to pressure tube failure prior to ballooning contact with the calandria tube. The values of the parameters considered in the assessment cover a very broad range and in many cases, values used are beyond the realistic range. However, the results for these cases are presented in order to understand which parameters dominate pressure tube behaviour following fuel element contact.

The parameters which have little effect on the calculated consequences of FE/PT contact are:

(a) Fuel to sheath heat transfer coefficient (5 to 25 kW/m²K);
(b) Neutron power transient (hot bundle or average);
(c) Initial bundle power (627 to 750 kW); and
(d) Number of pins in contact (1 or 3).

The most sensitive parameters relevant to FE/PT contact are:

(a) FE/PT contact conductance;
(b) Time of FE/PT contact, i.e., the difference between the time of contact and the time at which PT ballooning would occur in the absence of contact; and
(c) FE/PT contact width.

When the estimates presented for contact times (not less than 4 s) and contact width (0.015 mm) are considered, the minimum value of FE/PT contact conductance required for pressure tube failure is likely to be on the order of 10 kW/m²K or more. This critical value of contact conductance is...
judged to be sufficiently high that pressure tube failure due to FE/PT contact would not occur.

REFERENCES


EFFECT OF GAS FLOW IN THE INSULATING ANNULUS ON FUEL CHANNEL TEMPERATURES IN A SEVERE ACCIDENT IN A CANDU REACTOR

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ABSTRACT

The effect of gas flow in the insulating annulus on fuel channel temperatures in a severe accident in a CANDU reactor is analyzed. Results show that pressure tube and fuel temperatures following a severe accident could be reduced considerably using this approach. It is not necessary to provide cooling of the recirculated gas after passage through the fuel channel annulus, since the calculated temperature reductions of fuel bundles and pressure tubes result almost entirely from the improved conductance of the heat flow path to the moderator rather than from heat transport by the helium gas. The technical feasibility and economic justifications for the modification have not been assessed in this study. Such an assessment obviously would be needed before any further conclusions can be reached about this modification.

INTRODUCTION

In a CANDU reactor, heat loss from the hot coolant to the cool moderator is restricted, under normal operating conditions, by the gas-filled annulus between the pressure tube and calandria tube. The annular gas is nitrogen or carbon dioxide. Up to recently, the annular gas was stagnant, but following the Pickering-2 pressure tube rupture in 1983, a trickle flow is now provided. It is highly improbable that a dual-failure accident in which a large Loss of Coolant Accident (LOCA) is followed by failure of Emergency Coolant Injection (ECI) would occur. However, in the event of such an accident, fuel melting is prevented by heat flow across the insulating annulus to the separately cooled moderator (1,2), but damage to fuel bundles would occur. The heat flow rate across the annulus is enhanced by distortion of the pressure tube as its temperature reaches 800°C to 1000°C, either by sagging or by ballooning and thus coming into contact with the calandria tube.

An analytical investigation has been made of the effects on fuel and pressure tube temperatures in such a dual-failure accident sequence of a significant increase in the gas flow rate in the insulating annulus, and of the substitution of the normal annular gas by helium from the moderator cover gas. The effect of cooling the flowing helium outside the calandria vessel was also examined. The analytical model used assumes a normal configuration for the fuel bundle and pressure tube and thus treats the period before pressure tube distortion and severe bundle damage.

ANALYSIS

The analysis was done for a Bruce reactor unit, but it is equally applicable to any other CANDU reactor with suitable modifications in the program. It is assumed that just before the accident the reactor is operating at full power. For a Bruce reactor unit, 100% full power corresponds to a maximum Fuel Element Power Rating (FEPR) of about 48 W/cm² in the highest power channel, allowing for the maximum refuelling power ripple.

The rate of heat flow from the fuel to the inside surface of the pressure tube at any instant of time depends on the temperatures of the fuel and the pressure tube. During transient conditions, all these temperatures vary with time depending on the differences between the local fuel element heat sources and the rates of heat transfer between elements and to the pressure tube. At any instant of time, the model calculates the heat flow rates taking only the current temperatures into account. All the physical properties and emissivities are re-evaluated at each time step using the most recent values of temperatures. Axial flow of heat in the fuel channel components by conduction is neglected, being very small in comparison with the radial flow of heat. The small error this assumption introduces results in more conservative estimates of the temperatures.

The fuel bundle is represented by a series of concentric rings containing the same total mass of UO₂ and Zircaloy as in the actual fuel elements in the corresponding ring of the bundle, thus simulating the heat capacities of the rings of fuel elements (2,3,4). The radial distribution of the heat source among the fuel bundle rings is allowed for, and the model can be applied to any axial position allowing for the axial heat source variation. The ANS equation for heat-source decay is used.

Mechanisms of Heat Transfer

The fuel bundle is represented by concentric rings as described above. For the 37-element bundle used in Bruce reactor, four rings are used, consisting of four fuel nodes and seven sheath nodes. The model allows for conduction within the UO₂ fuel, contact conductance between the fuel and the sheath, conduction through the sheath and radiation and conduction through stagnant steam between the fuel-element rings and between the outer ring and the pressure tube.

The pressure tube receives heat from the fuel bundle and loses it to the annular gas. The mechanism of heat flow across the annulus is predominantly convection when the gas is flowing. Heat transfer by conduction is neglected for non-zero flow of gas and heat transfer by convection is neglected for zero gas flow. This assumption leads to some error in results at very low flows where heat transfer by conduction is comparable to heat transfer by convection. Thermal radiation between pressure tube and calandria tube is

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also accounted for. Axial heat transport by the flowing gas is allowed for.

The calandria tube receives heat from the gas by conduction or by convection and across the gas gap by radiation. It loses heat by convection or nucleate boiling to the moderator which is the ultimate heat sink.

All these modes of heat transfer are accounted for in the model.

Since the purpose of the analysis was to ascertain whether flowing helium in the annulus could prevent excessive fuel damage in such a severe accident the Zircaloy-steam reaction heat source was ignored in this study. Thus, results will only be valid for Zircaloy temperatures below about 1000°C.

The Program (ANNULUS)

A brief description of the computer program, ANNULUS, used in the analysis is given below.

The channel is divided axially into twelve nodal points, each nodal point corresponding to one fuel bundle. Average temperatures at each time step at the middle point of each node are calculated by the program. The program has two options for the temperature of the gas at the inlet of the first node. In one option, it is assumed that the temperature of gas entering the first node at any time step is equal to the temperature of gas leaving the last node at the previous time step. That is to say, there is no gas cooling outside the fuel channel. In the other option, it is assumed that the temperatures of the gas entering the first node at any time step is equal to a constant predefined value. This latter option simulates cooling of the annular gas in an external heat exchanger.

In the main time loop, the old values of temperatures are replaced by new values and the program proceeds to calculate temperatures at the end of the next time step. The values of temperatures are printed at predefined intervals. These calculations begin at an end-of-blowdown state as determined elsewhere and the results are printed at intervals of 10.8 seconds after that to correspond to the time interval used in IMPECC program (3,4). Depending on the time step and the length of time for which the run is desired, the number of steps required are estimated and read into the program.

The program can be used for the case of stagnant gas by putting the rate of gas flow equal to zero. It is capable of handling any desired flow rate of gas.

To help estimate practical gas flow rates, another program was written which estimates the pressure drop of the gas flowing in the annular space at different mass flow rates. Pressure drop in the annulus comprises entrance loss, exit loss, loss due to friction and loss due to change in momentum over the length of the channel. Coefficients of inlet and exit loss, geometrical parameters and other necessary information are read into the program. Properties of the gas and appropriate correlations for change in viscosity with temperature are also read in.

Since the program uses explicit finite difference equations in the formulations, great care has to be exercised to ensure that the program does not become unstable. After making a number of runs of the program, it has been found to be quite stable for the case of no gas flow, as is to be expected. Flow rates investigated in this analysis vary from zero to 0.027 kg/s (about 0.45 m³/s or a gas velocity of 200 m/s) through the channel. A check has been incorporated in the program to ensure stability at each iteration. If it becomes unstable, execution of the program stops and a message to that effect is printed. Calculations for the stagnant case are performed using a time step of 0.05 second and for the case of flowing gas, the time step has to be lowered to 0.0025 second to ensure stability for the maximum gas flow rate.

RESULTS AND DISCUSSION

Validation of the Program ANNULUS

The IMPECC program (2,3,4) developed at Carleton University has been in use for a long time and is well established. IMPECC is a comprehensive program which incorporates the fuel bundle model and the models of pressure and calandria tubes described here. It permits detailed analysis of the transient thermal behaviour of the fuel elements and pressure and calandria tubes following a LOCA and loss of EC1. The model allows for decay heat generation in the fuel elements and accounts for heat generation by the Zircaloy-steam reaction. IMPECC also allows for the non-axisymmetric conditions which result from the pressure tube sagging onto the calandria tube and the bundle slumping to the bottom of the pressure tube.

Following a LOCA, the fuel and the pressure tube temperatures begin to rise to a maximum and then decrease under the combined effects of heat source decay and heat transfer rates between components and to the moderator. This event is termed temperature turn-around. It is possible to run IMPECC until temperature turn-around with concentric channel geometry and without allowance for the Zircaloy-steam reaction. The results obtained from IMPECC under such conditions will then be standard to which results obtained from ANNULUS for the case of stagnant nitrogen can be compared.

IMPECC was run for these conditions assuming that the reactor power level was 50% just before the accident, corresponding to a Fuel Element Power Rating (FEPF) of 24 W/cm. ANNULUS was run for identical conditions and zero nitrogen flow. The results obtained from these two independent programs are compared in Figures 1 and 2. Figure 1 shows that at no
time does the difference between the maximum fuel temperatures predicted by these programs exceed 15°C. The agreement in Figure 2 between the maximum temperatures predicted for the pressure tube is even better. Towards the end of the transient, the values predicted by the two programs almost coincide. Such good agreement gives confidence in the validity of the program ANNUIS.

Results for Large LOCA and LOEC1 in a Bruce Reactor Unit

All the data presented in the rest of this paper are based on the assumption that the reactor power level was 100% full power, corresponding to 48.0 W/cm FEPF, just before the accident.

Figure 3 shows maximum fuel, pressure tube and calandria tube temperatures after the accident, assuming a helium flow rate of 0.015 kg/s (0.25 m³/s at 500°C) and assuming helium coolers are not operational. The fuel temperature reaches a peak value of about 1450°C after about 15 minutes while the pressure tube reaches a peak of about 1000°C somewhat earlier. There is very slight effect on the calandria tube temperatures. Curves for other flows are quite similar.

Figure 4 shows the effect of helium mass flow rates on the pressure drop through the annulus. At the maximum investigated helium velocity through the annulus (200 m/s) the pressure drop across the channel is 13.2 kPa, which is certainly a reasonably low value. Assuming the same helium flow rate for each of the 480 channels in a Bruce reactor unit, the total helium flow rate for the maximum annular velocity would be about 216 m³/s. Pumping 216 m³/s helium against a pressure differential of 13.2 kPa will require a pumping power of about 4.0 MW, assuming a blower efficiency of 75% and a motor efficiency of 95%. At lower flows, the pumping power is much less. The velocity of sound through helium at 500°C is about 1650 m/s. Thus there is little possibility of flow choking in the channels.

Figure 5 shows a typical pressure tube axial temperature distribution for the case of 0.015 kg/sec (0.25 m³ at 500°C) helium flow rate, 709.0 seconds after the initiating event. This time includes the time to the end of blowdown which is about 170 seconds for a large inlet-header LOCA. Figure 5 also
shows the effect of cooling the helium in external heat exchangers. It is seen that external cooling of helium does not make any perceptible difference to the maximum pressure tube temperature, although temperatures near the inlet are lower compared to the case of no external cooling. The small effect of external helium cooling on maximum pressure tube temperature occurs because the flowing helium acts mainly to improve the radial conductance of the gas gap rather than as a heat transport medium. The effect is shown clearly in Figure 6 which gives the axial temperature distribution of the helium gas in the annulus at 709 seconds. The gas temperature decreases towards the end of the annulus rather than continuing to increase. Cooling of the helium flow externally will not be needed to obtain the benefits of reduced fuel and pressure tube temperatures. Results at other helium flow rates are similar.

Figure 7 shows the effect of helium flow rate on the peak fuel temperature reached during the transient. As expected, with increasing helium flow, the peak fuel temperature decreases. Figure 7 suggests that increasing flows beyond about 0.020 kg/s (0.33 m³/s at 500°C), with a pressure loss of about 7.6 kPa, does not result in any great advantage. Figure 7 shows that the effect of external helium cooling on the peak fuel temperature is also negligible. It should be noted however, that some portion of the Zircaloy fuel sheaths will be above 1000°C under these conditions so that some Zircaloy-steam reaction would be expected which would increase the peak fuel temperature above the calculated values given here. This aspect of the problem requires further investigation.

Figure 8 shows somewhat similar results on the peak pressure tube temperatures. Again, cooling of helium in an external circuit is seen to have little effect, but the expected maximum temperature drops more or less in proportion to increasing helium flow. The helium flow rate suggested above results in predicted peak pressure tube temperatures of less than 900°C. At such temperatures, pressure tube distortion either by ballooning or sagging may not occur, so that the conditions assumed for the analysis would be valid. In any case, if the pressure tube did sag with helium flow in the annulus, the maximum fuel and pressure tube temperatures would be even less than those calculated here.

At the suggested design helium flow rate of 0.33 m³/s per channel, the peak fuel and pressure tube temperatures are considerably below the corresponding temperatures with stagnant CO₂ in the annulus for both types of pressure tube distortion, ballooning as shown in Figure 9 or sagging as shown in Figure 10.
CONCLUSIONS

From the above observations and discussions the following can be concluded:

(i) There is a good confidence in the results obtained from ANNUlus by virtue of the excellent agreement between results predicted by ANNUlus and IMPECC under identical conditions.

(ii) Substitution of nitrogen or CO₂ with flowing helium in the annular space decreases, substantially, the maximum fuel and pressure tube temperatures in a large LOCA accompanied by LOEC1, provided of course, that either no pressure tube distortion occurs or that the pressure tube sags. However, further investigation is needed on the effects of the heat produced by the Zircaloy-steam reaction.

(iii) Increasing the annular flow above a certain value is not helpful, but results in unnecessary increase of pumping power. As a first estimate a total flow rate of about 160 m³/s, which requires a pumping power of about 1.75 MW would appear to be adequate for benefits to be achieved in a Bruce Reactor unit.

(iv) Cooling of helium outside the calandria vessel has very little effect on the maximum fuel and pressure tube temperatures and is really not needed.

(v) Technical and economic feasibility must be shown before any further conclusions can be reached about this possible modification.

(vi) The purpose of this modification would not be primarily to reduce risk, which is already adequately low, but to reduce the financial costs of such an accident.

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REFERENCES


ABSTRACT

An alternative scheme is proposed to guarantee the safety behaviour of CANDU fuel channels under steam flow conditions. The proposed scheme is based on increasing the annulus gas gap effective thermal conductance during and after the accident. Two transient models (lumped and one-dimensional) are developed to predict the temperature of both pressure tube and calandria tube under the proposed scheme. The results show that the pressure tube temperature increases up to a maximum value after which it drops again. The pressure tube maximum temperature, at the channel vertical midplane, was found to be below 700°C, 500°C, and 430°C when the gap conductivity is 3.0, 4.5, and 6.0 W/m.K respectively. Calandria tube temperature is maintained at 110°C maximum. Therefore, the pressure tube - calandria tube contact is eliminated and a "Coolable Fuel Geometry" is guaranteed.

The moderator subcooling requirements are therefore relaxed and the moderator heat load is fully recovered to the first stage of feedwater heating system.

INTRODUCTION

The CANDU reactor consists of a large horizontal tank (calandria) which contains the low-pressure, low-temperature heavy water moderator. The calandria is penetrated by a large number of calandria tubes which provide "sites" for fuel channels, Figure 1.

The pressure tube in which the fuel is located is centrally located inside each calandria tube, and separated by a carbon dioxide filled gas gap. High pressure, high temperature heavy water coolant is pumped through the pressure tube to transfer the heat given off by the fuel to the steam generator. The annulus gas gap between the pressure tube and the calandria tube acts as a thermal insulator that minimizes heat losses from the coolant to the moderator during normal operation.

The present CANDU design has a potential safety feature that prevents the fuel melting under the postulated steam flow conditions. Steam flow conditions can occur in some of the reactor channels during either a postulated Loss Of Coolant Accident (LOCA) with critical break size that causes coolant stagnation or a LOCA associated with Loss Of Emergency Coolant Injection (LOCA + LOECI). Only two conditions are required to cope with this situation, reactor shutdown and continuous removal of the decay heat through the moderator system. With the present design (high thermal resistance of the annulus gas gap), most of the decay heat is transferred to and stored in the pressure tube. The pressure tube temperature rises quickly and the pressure tube strains to contact the calandria tube either by "ballooning", induced by steam pressure, or "sagging", induced by fuel weight. At contact, the sudden release of the large amount of the stored heat through a small contact area can cause calandria tube dryout (i.e. film boiling takes place at the calandria tube outer surface) when the moderator local subcooling is not sufficient. In this case, the calandria tube temperature rises quickly and the tube may fail causing reactor core damage.

Experimental study was carried out at WEIR and indicated that a moderator local subcooling of at least 33°C should be available prior to the accident in order to prevent calandria tube dryout. Economic penalties result from such moderator subcooling because of larger moderator heat exchangers required along with larger amount of heavy water hold up and pumping power. On the other hand, the moderator local temperature, and consequently the moderator subcooling requirements, are uncertain due to the contradicting results obtained by different authors [1 to 3] for the moderator circulation problem. In addition, there are some uncertainties with regard to the ranges of the contact area, contact conductance, and contact heat flux. Moreover, the transient Critical Heat Flux (CHF) was found by Mesler [4] to be about 50% less than the steady-state CHF usually used in CANDU fuel channel analysis.

The foregoing discussion serves to motivate the study that was performed here. A new concept for treating this safety problem is proposed as an alternative safety scheme. The proposed scheme and its safety and economic features will now be presented.
THE PROPOSED SCHEME.

The maximum decay heat flux at the beginning of the accident is much less than both steady-state and transient Critical Heat Flux even at zero subcooling. The problem arises from the fact that the high thermal resistance of the annulus gas gap forces the decay heat to be accumulated in the pressure tube instead of being transferred to the moderator as early as the accident starts. The proposed scheme, therefore, is based on increasing the annulus gas heat transfer during and after the accident. A highly reliable combination is to increase the thermal emissivity (ε) of the pressure tube and calandria tube (by surface coating) and to increase the equivalent thermal conductivity (kₐ) of the annulus gas gap by using circulated helium and additional spacers (see also the Appendix). The additional heat loss from the coolant to the moderator under normal operation, along with the moderator heat load will be fully recovered to the first stage of feedwater heaters.

The thermal behaviour of the pressure tube and calandria tube is presented first, while the moderator heat recovery scheme is presented later.

CHANNEL THERMAL BEHAVIOUR

A preliminary analysis of the channel thermal behaviour was carried out using a lumped model. In this model, the decay heat and channel temperature were assumed to be uniformly distributed along the channel length. The results of this model are shown in Figure 2 for both the present design (solid line) where ε = 0.2 and kₐ = 0.025 W/m.K and the proposed scheme (dotted lines) where ε varies from 0.5 to 1.0 and kₐ varies from 0.3 to 3.0 W/m.K. The results show that for the present design, the pressure tube temperature rises very rapidly so that in few seconds it reaches 1000°C. Increasing the equivalent thermal conductivity of the annulus gas gap, kₐ, significantly reduces the pressure tube temperature. The pressure tube temperature increases with time up to a maximum value after which it decreases again. The pressure tube maximum temperature could be controlled below 500°C when the equivalent thermal conductivity is above 3.0 W/m.K and the tubes thermal emissivity is above 0.5. These preliminary results show, in general, the validity of the proposed scheme to limit the pressure tube maximum temperature. More detailed analysis is given below using the one dimensional model.

One Dimensional Model

The fuel channel is assumed to be symmetric around the channel vertical midplane, Figure 1, so that only one half of the channel is modelled. The transient temperatures of both the pressure tube and the calandria tube are obtained from the energy balance of a ring element. The differential equations for both tubes are written as follows,

\[ \frac{\partial T_{PT}}{\partial t} = \frac{\partial^2 T_{PT}}{\partial x^2} + \frac{Q^0}{B_{PT}} - \frac{k_{PT}}{B_{PT}^3} (T_{PT} - T_{CT}) \]

\[ - \frac{\sigma F_{l2}}{B_{PT}} (T_{PT}^4 - T_{CT}^4) \quad (1) \]

\[ \frac{\partial T_{CT}}{\partial t} = \frac{\partial^2 T_{CT}}{\partial x^2} + \frac{k_{CT}}{B_{CT}^3} (T_{CT} - T_{CT}) \]

\[ + \frac{\sigma F_{l2}}{B_{CT}} (T_{PT}^4 - T_{CT}^4) - \frac{h}{B_{CT}} (T_{CT} - T_{mod}) \quad (2) \]

where

- \[ T_{PT} \] = Pressure tube absolute temperature, K
- \[ T_{CT} \] = Calandria tube absolute temperature, K
- \[ t \] = Time, s
- \[ a \] = Tube thermal diffusivity, m²/s
- \[ k_{CT} \] = Annulus gas thermal conductivity, W/m.K

Figure 2: Pressure Tube Transient Temperature Under Different Values Of ε and kₐ (Lumped Model)
$Q_{in}^* =$ Heat load, W/m²

$d =$ Tube thickness, m

$\delta =$ Annulus gap thickness, m

$k_g =$ Annulus gas gap thermal conductivity, W/m.K

$\sigma =$ Stefan Boltzman Constant W/m².K

$F_{12} =$ Gray body configuration factor

$B =$ PT & CT, Tube constant defined as

$B = \left( \frac{p^a c_d}{j/mMC} \right)$

$h =$ Heat transfer coefficient by boiling or convection, W/m².K

$T_{mod} =$ Moderator temperature, K

The initial and boundary conditions are written as follows:

$T_{PT}(0,x) = 300 \, ^\circ C$  \hspace{1cm} (3)

$T_{CT}(0,x) = 80 \, ^\circ C$  \hspace{1cm} (4)

$\frac{\partial T_{PT}(t,0)}{\partial x} = \frac{\partial T_{CT}(t,0)}{\partial x} = 0$  \hspace{1cm} (5)

$T_{PT}(t,L/2) = 300 \, ^\circ C$  \hspace{1cm} (6)

$\frac{\partial T_{CT}(t,L/2)}{\partial x} = 0$  \hspace{1cm} (7)

An explicit finite difference method is used to solve the above equations. The results of the pressure tube transient temperatures are shown in Figures 3 and 4. Difference values of $\epsilon$ and $k_g$ are also shown in the right hand side of each illustration.

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**Figure 3** Pressure Tube Transient Temperature

**Figure 4** Pressure Tube Transient Temperature
The results in Figure 3(a) show that with the present design values of $\epsilon$ and $k$ ($\epsilon = 0.2$ and $k = 0.025 \text{ W/m.K}$), the pressure tube temperature rises very rapidly so that pressure tube - calandria tube contact takes place. Increasing $k$ to a value of 3.0 W/m.K, Figure 3(b), limits the pressure tube maximum temperature to 690°C and the average temperature to 570°C. These maximum values occur after 50s from the beginning of the accident, after which the pressure tube temperature decreases with time. The effect of increasing channel emissivity was found to have less influence on both maximum and average temperatures, for example, increasing $\epsilon$ to unity, Figure 3(c), limits the pressure tube average temperature to 480°C.

Figure 4 shows the effect of rising $k$ from 1.5 W/m.K to 6.0 W/m.K while $\epsilon$ is constant. The effect is very apparent in reducing the pressure tube transient temperature significantly. The maximum pressure tube temperatures at the channel midplane are 825°C, 630°C, 505°C, and 430°C while tube average temperature are 705°C, 480°C, 400°C, and 375°C at $k$ equal to 1.5, 3.0, 4.5, and 6.0 W/m.K, respectively. In all these cases, the calandria tube transient temperature rises so that nucleate boiling takes place; after that, it rises very slowly. The maximum temperatures of the calandria tube (at the channel midplane) are shown in Figure 5 for some limiting cases.

![Figure 5 Calandria Tube Transient Temperature At Channel Midplane](image)

Advantages Of The Proposed Scheme

The main advantages of the proposed scheme can be summarized as follows:

i) The proposed scheme is simple, passive, and therefore has high reliability. No significant changes in the present design are added since the annulus gas circulation has been adopted for the new reactor designs. In addition, all thermal, mechanical, and nuclear characteristics of the fuel channel remain unchanged.

ii) The pressure tube maximum temperature could be controlled within a specified value so that pressure tube - calandria tube contact is eliminated. This significant safety advantage eliminates the concern of the calandria tube dryout and its possible failure. Therefore, the fuel channel integrity is preserved and a "Coolable Fuel Geometry" is guaranteed.

iii) Maintaining the pressure tube temperature low during the accident will provide a better heat sink for the fuel elements. Therefore, the clad temperature could be maintained at a lower value than that of the present design. This will reduce the Zircaloy-water exothermic reaction and its consequences on clad strength, heat generation, and hydrogen production.

Since the pressure tube - calandria tube contact is eliminated, the moderator subcooling requirements can then be relaxed, i.e. the moderator temperature can be increased. On the other hand, the apparent penalties of losing some of the channel energy will be recovered as given below.

MODERATOR HEAT RECOVERY (MHR)

A large amount of heat is generated within the moderator due to neutron thermalization (moderation) process. The moderator heat load represents about 4% to 5% of the reactor thermal energy and is about 120 MW(thermal) in CANDU 600. A moderator cooling system is, therefore, established for the continuous removal of this heat load. The hot moderator water is circulated through the moderator heat exchangers where its heat is transferred to the Recirculating Cooling Water (RCW). Eventually this heat is lost to the environment through the Raw Service Water (RSW). An additional amount of heat will be transferred to the moderator under normal operation using the above proposed scheme and also will be lost, Table 1.

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<th>$k_g$</th>
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Table 1 Additional Heat Losses, MW(thermal), to the Moderator during Normal Operation for CANDU 600 using Proposed Scheme
Economic and marketability disadvantages exist with the loss of such large amounts of energy to the environment, while the potential exists for utilizing this energy in the power cycle. The only attempt to recover this energy in power cycle, so far as the author is aware of, has been done by Todorvski and Hemmings [6], where the moderator energy is partially utilized. Their circuit has, however, two main disadvantages. The first is the penalties arising with the lost energy part (30% to 40%). The second is the complex control mechanism required to control the two types of heat exchangers simultaneously (namely, feedwater - moderator heat exchanger, and RSW - moderator heat exchanger).

To overcome these drawbacks, an alternative Moderator Heat Recovery (MHR) circuit will be presented below where all the moderator energy is utilized.

Figure 6 shows a typical CANDU 600 feedwater-condenser arrangement. The proposed MHR circuit is shown in Figure 7, where all moderator energy is transferred to the feedwater.

During normal conditions, all feedwater from the condenser flows through the moderator heat exchangers and then travels back to the other low-pressure feedwater heaters. For the period when feedwater heating is not required (or feedwater is not available), a steady (backup) RSW cooling system is provided to remove the heat load. The RSW system could be sized to remove the maximum heat produced during any expected abnormal (or part load) condition. To maintain the moderator outlet temperature (from the calandria) between the specified upper and lower limits, the following control system is provided:

a) to raise the temperature, the control valve C in the bypass line is opened.
b) to lower the temperature, one of the recirculating water pumps is started and the bypass control valve B and the RSW control valve A are opened.

The moderator temperature determines the degree of the opening of the control valves A, B and C. Figure 7 shows also a feedwater bypass line to allow bypassing the moderator heat exchangers, if necessary. A head tank would be provided in the circuit to cater for contraction and expansion of the fluids due to temperature variations. The MHR circuit would be isolated in the unlikely event of RSW leak into the feedwater. However, leakage, if any, should be into the RSW since pressures are higher on the feedwater side.

**Electrical Power Output**

Introducing the moderator heat load to the feedwater heating system will save some amount of the bleeding steam required for feedwater heating. The saved amount of bleeding steam will then be used to produce additional electric power in the steam turbine. The steam cycle parameters, Figure 6, are calculated first using an iterative procedure. The important calculated parameters are listed in Table 2.

**Figure 6 CANDU Secondary Side**

**Figure 7 Proposed MHR Circuit**

**Table 2 Resultant Parameters For Steam Cycle of CANDU 600 Reactor**

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\dot{\vartheta}_{\text{b}}$</td>
<td>1043.0 kg/s</td>
</tr>
<tr>
<td>$\dot{\vartheta}_{\text{c}}$</td>
<td>43.4 kg/s</td>
</tr>
<tr>
<td>$\dot{\vartheta}_{\text{b}}$</td>
<td>27.5 kg/s</td>
</tr>
<tr>
<td>$\dot{\vartheta}_{\text{c}}$</td>
<td>32.5 kg/s</td>
</tr>
<tr>
<td>$\dot{\vartheta}_{\text{feed}}$</td>
<td>732.0 kg/s</td>
</tr>
<tr>
<td>$p_a$</td>
<td>0.03 MPa</td>
</tr>
<tr>
<td>$p_b$</td>
<td>0.07 MPa</td>
</tr>
<tr>
<td>$p_c$</td>
<td>0.16 MPa</td>
</tr>
</tbody>
</table>
It was found that the heat transferred to the first Feedwater Heater (FWH) by the bleeding steam is less than the moderator heat load. Therefore, the first FWH can then be eliminated and the amount of bleeding steam required to the second FWH \( (\dot{m}_b) \) can also be reduced to \( (\dot{m}_b') \) calculated as follows:

\[
\dot{m}_b' = \dot{m}_a - \dot{m}_b - \dot{m}_c - (\dot{m}_a - \dot{m}_b - \dot{m}_c) \left( \frac{1}{l_b} - \frac{1}{l_i} \right)
\]

The electric power produced by the saved amount of the bleeding steam is calculated as follows:

\[
\text{Power} = \dot{n}_a \left( l_a - l_i \right) + \left( \dot{n}_b - \dot{n}_b' \right) \left( l_b - l_i \right)
\]

where, \( l_a, l_b, l_c, l_i, \) and \( l_g \) are the specific enthalpy of the steam at points a, b, c, 5, and 8 respectively and \( \dot{m}_a, \dot{m}_b, \dot{m}_c \) are the bleeding steam flow rates at points a, b, and c respectively. The first term in equation (9) represents the power produced from the bleeding steam of the eliminated first FWH, and the second term represents the power produced from the saved steam of the second FWH.

\[\text{Figure 8 Electric Power Produced}\]

**Figure 8** shows the resultant power produced as a function of the moderator heat load, \( T_{\text{mod}} \). The figure shows the total electric power produced by the recovered moderator heat load (dashed-dot line) which increases as the heat load increases. These values are, however, reduced by an amount equal to the thermal energy lost to the moderator from the fuel channels times the plant thermal efficiency (only when the moderator heat load is greater than 120 MWch).

Other Advantages of the MHR Circuit

The outstanding features and advantages of the proposed MHR circuit are summarized as:

i) The MHR circuit is simple and therefore has good reliability and requires low maintenance.

ii) At part load, the proposed circuit is self-regulating. Since the feedwater flow rate is proportional to the reactor power, the feedwater temperature will remain constant at all reactor powers.

iii) The RSW heat exchangers used as a backup are relatively small. This is due to the high logarithmic mean temperature difference between the moderator and RSW.

iv) The feedwater is clean, so there should be no problem of fouling and corrosion in the moderator heat exchangers.

v) Leakage in the moderator heat exchangers, if any, would be from feedwater to moderator in view of existing pressure differences. Thus the feedwater system would not be contaminated.

vi) The utilization of moderator energy and the increase in the cycle efficiency will add marketing advantages to CANDU systems. The moderator heat will also be eliminated from the loss side of the CANDU heat balance sheet.

CONCLUSION

A simple, and reliable scheme is presented as an alternative solution to the fuel channel behavior under steam flow conditions. The proposed scheme is based on increasing the annulus gap gap effective thermal conductivity and the channel thermal emissivity; partially under normal operation and fully during the accident. Therefore an early and continuous transfer of the channel decay heat to the moderator takes place during and after the accident. The maximum pressure tube temperature is limited so that pressure tube-calandria tube contact is eliminated. Therefore, the channel integrity is preserved and a "coolable fuel geometry" during and after the accident is guaranteed. The heat loss from the fuel channels to the moderator during normal operation along with the moderator heat load is recovered. A moderator heat recovery circuit is presented for full recovery of the moderator energy to the first stages of feedwater heating system. Safety, economic, and marketing advantages exist with the proposed scheme over the present design. With the proposed scheme, the CANDU reactor will have a safety feature which no other power reactor in the world can claim.
REFERENCES


APPENDIX

Selected Methods To Increase kg

1. Combined Convection
   a) Replacement of CO₂ by helium
   b) Continuous circulation of helium during and after the accident
   c) Increase the velocity of the gas flow
   d) Swirl flow is preferred
   e) Some additives may be added to the gas during the accident to increase kg.

   It is worth mentioning that combined free and forced convection increases by ten times the gas thermal conductivity as given in Ref. [7].

2. More Conducting Spacers

   a) Introduce more conducting spacers to the fuel channels with suitable number and distribution so that conduction and convection become more efficient while nuclear characteristics do not change.

   b) The spacers should be arranged in such a way that they are in contact only with the calandria tube during normal operation. This will reduce the heat losses from fuel channels during normal operation. Contact of the spacers with pressure tube should be only during the accident.
CATHENA SIMULATION OF THE WOLSUNG D$_2$O SPILL INCIDENT OF 1984 NOVEMBER 25

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ABSTRACT

The CATHENA code (formerly ATHENA) has been used to simulate the thermal hydraulic behavior of the WOLSUNG-1 CANDU-600 reactor during the D$_2$O spill incident of 1984 November 25. A 4-inch (nominal) Liquid Relief Valve (LRV) inadvertently opened in the reactor auxiliary system during normal reactor operation, resulting in a discharge of heavy water from the primary heat transport system. The valve remained open for approximately 29 minutes.

CATHENA is an advanced thermal hydraulic computer code for postulated loss-of-coolant accidents (LOCA) and transient faults in CANDU nuclear reactors. A full two-fluid (six-equation) representation of the two-phase flow is used. Component models are used to represent pumps, valves, critical discharge, etc., which are necessary to describe the behavior of the CANDU system under upset conditions. Heat transfer between the fluid and piping walls or fuel) is modeled using applicable correlations for boiling, condensation and convective heat transfer.

The two, figure-of-eight, primary heat transport (PHT) loops of the reactor were modeled with CATHENA using initial conditions and relevant boundary conditions derived from actual plant data. The CATHENA simulation of the event showed excellent agreement with available plant data.

INTRODUCTION

On 1984 November 25, at a reactor operating power of 100%, a liquid relief valve (LRV) inadvertently opened in the WOLSUNG-1 CANDU-600 reactor. This valve remained open for almost a half hour - until the condition causing the open valve was rectified. The resulting loss of primary heat transport (PHT) coolant caused depressurization and voiding of the PHT system of the reactor. This event was simulated with the CATHENA code, as part of a continuing verification program.

CATHENA (Canadian Algorithm for Thermal hydraulic Network Analysis) is a two-fluid computer code developed primarily to analyze postulated LOCA's in the CANDU reactor system. CATHENA uses a flexible input data format, in which test facilities and reactor systems may be modeled with ease. A computationally efficient finite difference method is used to solve the hydrodynamic equations, with mass conservation enforced using a truncation error correction scheme [1]. Component models necessary for describing a CANDU system under LOCA conditions have been incorporated into the code.

The two figure-of-eight, PHT loops of the WOLSUNG-1 reactor were modeled. Header-to-header interconnects, feedwater lines, lines to the pressurizer, and lines to the degasser condenser were also included in the idealization. Initial conditions for the simulation were obtained from plant operating data prior to the event. Relevant boundary conditions for the simulation were also derived from plant data.

This paper describes the CATHENA idealization of the WOLSUNG-1 reactor and the calculation of the D$_2$O spill event, and compares the simulation results with available plant data.

THE WOLSUNG-1 D$_2$O SPILL INCIDENT OF 1984 NOVEMBER 25

On 1984 November 25, at 100% full power, technicians were testing the pressure setpoint of a LRV. During the test, a short circuit was accidently created which blew a fuse on one of three channels of control logic for the LRV. Coincident with this, an unexpected fault existed on another channel. The LRV was designed to open on a loss of power in two channels; therefore, it opened and remained open.

Heavy water discharged from the primary circuit through the open LRV to the degasser condenser. A reactor trip occurred, due to low pressurizer level, within one minute of the opening of the LRV. The discharge of coolant also led to depressurization of the primary circuit down to the loop isolation pressure setpoint about eight minutes after the LRV opened. The loop isolation signal initiated the closing of feed valves, bleed valves and pressurizer line valves, which connect the two primary circuit loops. A minute after loop isolation, the operator reopened the feed isolation valves to provide makeup for the PHT.

Discharge continued through the loop to the degasser condenser. From there, the coolant passed through a cooler to the D$_2$O storage tank. The storage tank inventory increased until a rupture disk burst about 15 minutes after initiation of the transient. Approximately 24 Mg of PHT coolant (1/4 of the primary circuit inventory) spilled from the storage tank. Twenty-nine minutes after the LRV opened, it was closed by operators replacing the burnt out fuse. Discharge from the primary circuit ceased. The operators initiated a cooldown of the primary circuit. First, the secondary side was allowed to cool down by boiler pressure control. Second, the shutdown cooling system was initiated for long-term cooling.

No evidence of fuel failures was found after the event.
Hydrodynamic Equations

The two-fluid equations used by the CATHENA code are based on the one-dimensional conservation equations for mass, energy and momentum developed by Delhaye [2] for two-phase flow. Constitutive relations used to calculate the interphase and fluid-to-wall transfer terms in the conservation equations depend on the flow regime, which is predicted using a flow-regime map.

Constitutive Relations

A separate flow-regime map is used for horizontal flow, and for vertical or inclined flow. For horizontal flow, it is assumed that the flow pattern is either stratified, mixed, or transitional between the two states. In stratified flow, the gas and liquid are assumed to travel in separate streams in the top and bottom parts of the pipe, respectively. In mixed flow, one phase is assumed to be dispersed as small particles within the second phase. Between these regimes, a transitional state is assumed, which approximates the region of intermittent flow.

In vertical or inclined pipes, a similar approach is used. It is assumed that the flow is either annular, mixed, or transitional between the two states.

Heat Transfer

CATHENA is able to calculate the radial temperature distribution in a large number of solid elements at each thermalhydraulic location. The solid elements can represent individual fuel pins in a pin bundle, or different circumferential sectors in a pipe wall. The ability to perform a separate heat-conduction analysis for each solid element is particularly important for calculating heat transfer in a horizontal CANDU fuel channel, where stratified flow is present. Validation tests have demonstrated that CATHENA is able to predict fuel behaviour under stratified and near-stratified flow conditions (see, for example [4]).

CATHENA also can calculate radiation heat transfer in the fuel assembly. As well, heat generated from the steam-zirconium reaction may be included in the calculation.

Heat transfer can occur between the liquid and the steam phases, and also between each phase and any solid surface the phase contacts. Applicable correlations for boiling, condensation, and forced convective heat transfer are used for this calculation.

Numerical Method

The numerical solution scheme used in CATHENA has been designed for stability, robustness, and economy. A conventional staggered-grid, finite-difference approach is used with the pressure, phase enthalpies, and void fraction evaluated at cell centres, and phase velocities evaluated at cell boundaries. This is illustrated in Figure 1.

The formulation of the semi-implicit finite-difference equations [3] has eliminated the material Courant limit. This allows larger time-steps during slow transients, and thus increases the computational efficiency. Mass conservation is enforced using a truncation error correction term similar to RELAP/MOD2 [4]. This is particularly important in the prediction of long transients such as this WULSUNG event, where the accumulation of small mass errors at a large number of time-steps can reduce solution accuracy.

Component Models

Supplementary models have been included in CATHENA to describe the system components required to represent a CANDU heat transport system. Component models included at present are pumps, valves/orifices, abrupt area changes, a pressurizer, a break discharge, and a simple generalized control system. Also, most input variables can be made time dependent.

A separate component model for steam generators is not required, as the two-fluid model automatically accounts for boiler phenomena.

CATHENA SIMULATION OF EVENT

Idealization

The two, figure-of-eight, PHT loops of the reactor were modelled (see Figure 2). Header-to-header interconnects, loop interconnect, lines to the pressurizer, and lines to the degasser-condenser were also included in the idealization. In total, 134 nodes, 138 links, and 262 heat transfer surfaces (to piping and fuel) were used in the calculation (see Figure 3).

Four average channels were modelled representing the 4 x 95 channels of the reactor. Each channel was modelled using five representative pins at different channel elevations, as seen in Figure 4. This is necessary to predict individual pin temperature histories in the presence of stratified flow.

Flow through the LRV was calculated using the valve specifications of the manufacturer, and a standard subcritical/critical discharge model. Critical discharge was predicted throughout the transient. The secondary sides of the steam generators were modelled as boundary conditions.

Initial Conditions

Initial conditions prior to the spill event were taken from plant data. The plant was operating at 100% power, the PHT flow was single-phase liquid (2115 kg/s), and the outlet header pressure was 10.0 MPa. The loop flow resistance was obtained from reactor design data, with the resulting circuit pressure profile generated by CATHENA verified against plant design data.
### Boundary Conditions

Boundary conditions, such as valve opening/closing times, and D.O feed into the PHT, were derived from plant data. The secondary side of the steam generators was modeled using plant data for the temperature, and appropriate heat transfer coefficients for full power/decay power operation. The power transient of the reactor, with the trip assumed at 42 s, was obtained from a separate calculation and is shown in Figure 5. Table 1 gives the timing of significant events in the transient.

#### TABLE 1: TIMES OF SIGNIFICANT EVENTS

<table>
<thead>
<tr>
<th>Time (s)</th>
<th>Event</th>
</tr>
</thead>
<tbody>
<tr>
<td>0 s</td>
<td>Liquid relief valve opens</td>
</tr>
<tr>
<td>42 s</td>
<td>Reactor trip</td>
</tr>
<tr>
<td>538 s</td>
<td>PHT loop isolation</td>
</tr>
<tr>
<td>612 s</td>
<td>D.O feed loop interconnect closes</td>
</tr>
<tr>
<td>1314 s</td>
<td>Steam generator medium rate cooldown</td>
</tr>
<tr>
<td>1734 s</td>
<td>Steam generator maximum rate cooldown Liquid relief valve closes</td>
</tr>
</tbody>
</table>

At 538 s, the loop isolation valves were closed. However, the feed isolation valves remained open, except for the period from 538 to 612 s. Feedwater flowed into the PHT system between 42 and 538 s, and also from 2033 s until the end of the transient.

### SIMULATION RESULTS

In this section, the results of the CATHENA simulation are discussed and compared with available plant data. The simulation was continued for almost an hour of real time, until it was determined that no temperature excursions in the fuel pins were predicted. The calculation was performed on a VAX 11/750 and required about 11 hours of equivalent CPU time on a CDC CYBER-175 for 3000 s of simulation.

**PHT Pressure.** Figure 6 shows the CATHENA-calculated pressure at outlet header 7 (nearest the open LRV) compared with plant data.

Only slight depressurization of the PHT system is observed until the reactor trip (42 s), followed by a rapidly dropping pressure until about 500 s. The depressurization occurred as the steam generators removed heat from the PHT. After 500 s, system pressure was largely controlled by the secondary-side saturation temperature. At about 1300 s, cooldown of the steam generators was initiated and the system pressure continued to drop, controlled by secondary-side conditions.
The slight underprediction of pressure at about 500 s is thought to be due to an overestimation of the boiling heat transfer coefficient for the steam generator during this period. However, overall agreement with plant data was very good.

Only one value of inlet header pressure was available from recorded plant data. A value of 6.5 MPa at approximately 900 s. The CATHENA prediction was 6.0 MPa.

Pressurizer Level. A non-equilibrium two-region pressurizer model was recently developed for CATHENA and this simulation was useful in the validation of this model. Figure 7 compares the CATHENA calculation of the pressurizer level with plant data. Close agreement is observed, with the pressurizer emptying at about 170 s.

PHT Inventory. Figure 8 shows the predicted total PHT inventory in each figure-of-eight loop during the transient. Initially, the inventory increased after the trip, due to the cooling of the PHT circuit and the draining of the pressurizer. The inventory
remained almost constant until 538 s, with feedwater flow almost balancing discharge from the LRV. After 538 s, loop 1 lost inventory due to discharge from the LRV while loop 2 retained its inventory. Minimal flow between the two loops was predicted in the feed lines after 538 s, because of the small pressure difference between the inlet of P4 and P2 (see Figure 3). Pressures at these locations were set mainly by similar secondary-side conditions in SG2 and SG4. When the LRV was closed at 1734 s, inventory in loop 1 increased due to D2O feed makeup.

Fuel Temperatures. CATHENA did not predict rising fuel temperatures in any of the channels. Maximum fuel temperatures would be expected in loop 2, closest to the break. Figure 10 shows the sheath and fuel centreline temperatures at the middle of channel 3, for the uppermost representative pin (1). Temperatures did not rise during the transient. This was mainly because (1) the pumps maintained a flow high enough to prevent flow stratification (see Figure 9), and (2) heat decay was low during the latter part of the transient.

Since these results assumed an "average" channel, a header-to-header calculation for a 7.3-MW channel was also performed, using header boundary conditions derived from the full system calculation. Rising fuel temperatures were also not predicted in this case.

No evidence of fuel failures was found at the plant.

SUMMARY

(1) The CATHENA code has been briefly described.

(2) A CATHENA idealization of the Wolsung-1 reactor, using plant data for initial and boundary conditions has been given for the D2O spill incident of 1984 November 25.

(3) The CATHENA simulation results have been discussed, and compared with available plant data. Agreement was very good.

(4) The CATHENA code is able to accurately predict the fuel heatup behaviour in a CANDU channel under LOCA conditions. No significant temperature rise in the fuel was predicted for this event. This was supported by plant data.

ACKNOWLEDGMENT

The authors are grateful to Dr. Won-Pyo Chang, of the Korean Advanced Energy Institute, for the many helpful discussions, and suggestions made, during the preparation of the CATHENA idealization for the Wolsung-1 reactor.
REFERENCES


(4) RANSOM, V.H., "RELAP/MOD2: For PWR Transient Analysis", CNS/ANS International Conference on NUMERICAL METHODS IN NUCLEAR ENGINEERING, Montreal, Canada, Sept. 6-9, 1983, pp. 40-60.
SESSION F: ECONOMIC AND SOCIAL ISSUES

Chairman: T.R. Lassau, Ont. Research Foundation

The role of R&D in facilitating industrial use of radiation processing.
S.L. Iverson, AECL WNRE

Energy and exergy analyses of a nuclear steam power plant.
M.A. Rosen and D.S. Scott, University of Toronto

Innovative design and construction methods to reduce nuclear plant construction time.
P.D. Stevens-Guille, W.J. Penn and N. Fairclough, Ontario Hydro

The necessity for nuclear power: the oxygen-CO₂ balance.
J.W. Harding

Frightened at false fire: nuclear energy, the news media and the public.
D. Mosey, Ontario Hydro
ABSTRACT

Radiation Applications Research Branch was formed by AECL to develop and help industry apply new applications of ionizing radiation. The current major industrial uses of such radiation, sterilization of medical disposables and cross-linking, grafting and polymerization, were developed primarily by, or in close cooperation with, industrial users. Major opportunities exist today to broaden the range of products pasteurized or sterilized by radiation and to extend cross-linking and polymerization into bulk materials and large components. Collaboration between experts in user industries and radiation scientists is proposed as a cost and time effective method of exploiting these opportunities.

INTRODUCTION

In 1985 March, Atomic Energy of Canada Limited (AECL) formed a new Branch, Radiation Applications Research, to conduct R&D in support of existing industrial radiation processing and to develop and support new radiation applications. Libraries are full of books and research reports on uses of radiation, ranging from preserving food to improving the quality of gem stones, and R&D continues in many labs around the world. How can a new player be most effective in getting these applications out of the libraries and labs and onto the factory floor? The approach we have adopted is to focus the R&D program sharply on solving, in cooperation with industrial partners, relatively simple problems standing in the way of major applications. We expect this approach to be successful because, generally, the scientific aspects of many radiation processes are well developed. What is needed is a vehicle to transfer this technology into the plant or factory where its advantages can be exploited. Our role is to facilitate this transfer.

To help design the vehicle, we have examined how radiation processing technology has been successfully transferred in the past. One of the major industrial uses of radiation is sterilization of medical disposables. More than 135 plants exist in 42 countries, with the capability to treat about 2.3 million cubic metres of product per year, mostly using gamma radiation from 60Co. During the 25-year period from the practical beginning of this application to the present, significant problems were overcome, mostly by the medical products industry itself. Key developments were the demonstration that microorganisms could be inactivated without causing the product to become toxic, the development of radiation-resistant materials for manufacture of medical disposables, the streamlining of regulations and, in some cases, regulatory concern about the safety of competing processes. These developments allowed radiation processing to develop to a scale where it was economically competitive.

Major opportunities exist today to broaden the range of products sterilized, pasteurized or disinfected using radiation, partly because the chemicals traditionally used for these processes are being slowly phased out due to their toxicity to humans. Products often mentioned range from food items, such as chicken for the reduction of salmonella bacteria and extension of shelf life or stored wheat for the inactivation of insects, to industrial products, such as wood chips for the prevention of decay during storage, and sewage sludge to inactivate viruses and bacteria to allow unrestricted recycling as a fertilizer. Significant research effort has been expended in all these areas and the technical objectives appear achievable. R&D is required to optimize local varieties and conditions, to tailor treatments for industrial-scale production demonstration and, in some cases, to meet regulatory requirements. However, before commercial success is achieved and each application must pass through a series of barriers similar to those successfully negotiated by medical products.

To negotiate these barriers successfully, a radiation process should offer a distinct technical and economic advantage, meet a consumer need, be acceptable to regulatory agencies, and be developed and funded primarily by industry. Our initial efforts are being concentrated in areas offering the fewest barriers and the greatest number of advantages.

A second large area of industrial radiation processing is in cross-linking, grafting and polymerization. Products include heat-resistant plastic insultation for electrical wires, permanent-press fabric, heat-shrinkable plastic film, and radiation-cured paints and inks. Nearly all of the radiation is delivered by electron accelerators operating at a few MeV or less. These applications offer very strong technical advantages, clearly meet consumer needs, offer few, if any, regulatory problems and were developed and funded almost entirely by industry. Development of industrial linear accelerators delivering inexpensive radiation at higher energies offers opportunities to extend these and similar applications into bulk materials and larger components. An example could be lightweight plastic components for automotive or aerospace applications with strength, heat or pressure resistance improved by radiation processing.

A third area is the use of radiation to synthesize or break down chemicals. An example is the use of radiation to destroy PCBs. While many such applications have been described, few are in use, mainly because similar effects can be achieved more cheaply by other means.
A key component in the success of current industrial applications has been the strong and continuous involvement of industry users from the early stages. A successful project must pass through at least six stages and we believe that industry must be involved in all six. The stages are:

1. Tentative Acceptance
2. Technical Feasibility
3. Economic Feasibility
4. Risk Reduction
5. Commit Production Facility
6. Operate Production Facility

These stages are similar to those listed by Bakhin, although the emphasis is somewhat different. This may be a result of differences in the process of industrial adoption of new technology among countries.

Technically strong companies willing to commit significant resources can complete the entire process without assistance but in most cases a collaborative effort between experts from the user industry and radiation scientists is cost and time effective. Well-known and developed applications require very little time in the preliminary stages. Applications that prove to be less promising must be identified and dropped as soon as possible.

The first stage, Tentative Acceptance is the recognition that radiation processing may be a solution to a specific industrial problem and the decision to commit resources for further assessment of its possibilities. Three themes run through this stage all of which evolve throughout. The first and most difficult is identifying and clarifying the problem. While this sounds simple it frequently happens that people such as the plant manager, an R&D person, or a supervisor on the plant floor understand and communicate a situation very differently. This may even reach the point where one believes a problem is very important while another does not recognize its existence. An example of this is the decay of wood chips in storage piles at pulp mills. Pulp industry R&D personnel are aware that significant wood mass is being lost in this way, while plant personnel are not convinced that it occurs, or are not convinced that it is effective. The first step is to agree and describe the effort that is required to find a solution. By talking to a number of people, perhaps at a number of different plants it is usually possible to get an understanding of the frequency and dollar value of the problem and to determine if it is industry-wide or specific to a particular plant. In the food industry in particular it is necessary to build a certain amount of trust because information about conditions such as insect infestation or spoilage can have a negative effect on consumer acceptance. Regulatory, competitive position and general mistrust of the nuclear industry are other roadblocks that sometimes need to be overcome before frank communication can occur and the problem can be understood and evaluated.

As the problem becomes clearer it is possible to form a perception that radiation processing might be applied (sterilization, pasteurization, disinfection, crosslinking or other chemical change), and to estimate that costs and side-effects will be manageable. Judgment is very important at this stage. A person with wide knowledge of radiation processing and its scientific basis can make a major contribution since pursuit of unpromising applications limits the effort that can be put into ones with more promise.

The second major theme is ensuring that the right people are involved. On the side of industry, it is important to involve action-oriented people with something to gain and enough authority to commit required resources. We have had good results working with plant managers and new product development managers. It is important to involve R&D people from the user industry, if possible, since they will often be expected to comment on the proposed new process.

The desirability of introducing additional parties into the discussion at this stage should not be overlooked. If the problem is industry-wide and competitive advantage is not a primary concern, it can be very helpful to introduce people from an Industrial Council, Association or Institute into the project. An example of this is a case where such a group is supporting work aimed at overcoming problems encountered during shipment of lumber for export. If additional technical support is required, university researchers, scientists from government departments (such as Agriculture Canada), consultants or suppliers of appropriate radiation equipment can be brought in.

The third major theme in the gradual education of the potential user about radiation processing. A general understanding of the field must be built up before it is useful to go into much technical detail. Potential users, however, are likely to have technical concerns about the effects of radiation on important properties of their materials. These concerns should be addressed in detail if data exists, but at the very least must be clearly identified for study during stage two, technical feasibility. It is also helpful to inform potential users about the current major industrial uses of radiation since these can be used as models of what must be done for success.

If stage one has been completed successfully, appropriate people in the user industry, and other associates as required, will have built up an understanding of the benefits and limitations of radiation processing and how it could be applied to a particular problem in their industry. They will also have identified information that must be obtained before a major commitment could be made and be in a position to commit resources to obtain that information.

The purpose of stage two Technical Feasibility is to reach the recognition by the potential user that radiation can in general achieve the desired effect in his product without major negative side-effects. The first step is to agree and describe the effect desired and the important properties of the material. In some cases, sufficient information will exist in the literature to form an expert opinion, while in others R&D will be required. Informed judgment of a technical expert is extremely useful both in evaluating the information in the literature and in designing an appropriate R&D program.

If R&D, or a small demonstration, is required it is our belief that the potential user should provide a significant proportion of the resources. The most common arrangement is for the user to provide the product, we perform the irradiation and provide dosimetry; the user then tests the product to ensure that it meets their requirements. We, or they,
perform additional chemical, physical or microbiological tests as required; and we often provide an interpretation of the results in the context of the radiation processing literature. All results are equally shared and can be exploited by either party. Alternatively, if a potential user wishes to have exclusive rights to the findings they can do so by paying for our services.

We currently use a gamma cell at the Whiteshell Nuclear Research Establishment (WNRE) for most of our irradiations and have access to various electron accelerators and larger gamma irradiators as required. In 1987 March our ability to irradiate material will increase significantly with the commissioning of a 1-kW 10 MeV industrial electron accelerator at WNRE. This machine will be equipped with a conveyor and be capable of irradiating significant amounts of material (about 400 kg/h at a dose of 0.5 Mrad for example) for product testing and for demonstration purposes. It will also be used for static irradiations and irradiations of liquids and gases in circulating loops.

All technical questions do not have to be answered completely for the successful completion of stage two, but parties should agree that the required dose is known and the probability of technical success is very high. The objective is to get through stage two as quickly as possible with the expenditure of as little money as possible. In our experience many projects can be brought to this stage in a few months for an expenditure of a few tens of thousands of dollars.

Stage three Economic Feasibility ends with the decision by Industry that they can expect (assuming successful negotiation of the remaining barriers) to profit from an investment in radiation processing. The involvement of equipment manufacturers may be needed to help estimate the cost of a facility designed to achieve desired throughput at a given dose. The major part of this analysis must be done by the potential user although we can provide some typical unit product costs.

Stage four Risk Reduction involves answering the necessary questions before funds can be committed for a production facility. The risks normally fall into one or more of five areas:

1. technical
2. regulatory
3. consumer acceptance
4. economic
5. business

Technical risks are usually addressed by conducting R&D to answer specific questions. Depending on the situation, the potential user may develop their own expertise by conducting the R&D or they may wish to contract some or all of it out. It may also be necessary to build a pilot-scale facility if the technical risks fall mainly in the area of materials handling or dose distribution in the product. Our role could range from the main contractor for the R&D to sub-contractor for particular aspects to expert consultant.

Certain products, such as food, need regulatory approval before they can be offered for sale. Submissions for such approval often require data from efficacy tests, which show that the desired effects can be achieved with large amounts of material in an industrial scale plant. Approval in food-item specific so once an item is cleared it can be produced in other plants and by other companies. Approval is granted by national authorities on exported food requires clearance in the country where it is sold. A joint program of the Food and Agriculture Organization of the United Nations and the World Health Organization called the Codex Alimentarius Commission has written a general standard for irradiated foods and a code of practice for the operation of radiation facilities. An important conclusion of the Commission is that the wholesomeness of foods irradiated at an overall average dose of 10 kGy or less is not impaired and that toxicological testing of these foods is not required. Applications such as sprout inhibition, disinfection, shelf life extension and reduction of microbiological load fall below this dose, while higher doses are required to produce sterile or shelf-stable foods. It has been noted that international trade in irradiated foods would be facilitated if legislation passed by countries was consistent with the Codex standard.

Also in the food area, Consumer Acceptance may be perceived as a risk requiring attention. Information is available in the international literature from South Africa where irradiated foods have been test marketed with some success. It is likely, however, that a market study followed by marketing tests may be required for at least the first few food items introduced in each country or area.

Economic risks may also be present for applications where the treatment cost or through-puts are not well known. It may be possible to better estimate costs by a major in-house study or by employing a consultant. The building of a pilot plant may be desirable particularly if it can also contribute to technical and marketing objectives.

Finally, the business risks and the structure of the radiation business need some consideration. For medical disposables, a significant part of the business is done by custom irradiation facilities although some manufacturers have their own irradiators. Low energy electron accelerators used for cross-linking are all owned and operated on a dedicated basis because they are closely tied to the remainder of the process. Volume of product and intimacy of linkage in the remainder of the process are the main factors to be considered in reaching a decision.

It may be necessary to treat several of these issues before stage five Commit Production Facility can be reached. Radiation Applications Research would typically have little or no involvement during design and construction except perhaps in areas of shielding calculations or preparation of licensing documents if desired by a client.

Process trouble shooting and extensive dosimetry are two areas where our expertise could be useful during commissioning. The final stage is Operate Production Facility. In this stage little assistance would be required by technically strong users that had developed process support capability. The option would be available however to utilize Radiation Application Research Branch for assistance with trouble shooting, testing effects on new products or materials, non-routine dosimetry or other technical issues required to keep the facility productive.
CONCLUSION

We believe that radiation processing can contribute to competitiveness and profitability through the creation of new products and solution of existing problems in a number of industries. While large and technically strong organizations can develop the expertise to independently assess and exploit these opportunities, results can often be achieved more quickly at lower cost by a joint team including experts from Radiation Applications Research Branch of AECL. By cooperating closely with industry in all stages from idea identification through support of production facilities, we can help realize the benefits that can be provided by radiation processing.

REFERENCES


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ENERGY AND EXERGY ANALYSES OF
A NUCLEAR STEAM POWER PLANT

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ABSTRACT
Thermodynamic analyses of a nuclear steam power plant are presented. The analyses, which are based on both the first and second laws of thermodynamics, were performed using a process-simulation computer code which had previously been enhanced by the authors for energy and exergy analyses. The results yield some interesting new insights into the performance of nuclear steam power plants, and could prove useful to the designers of nuclear-related, and other, technologies. The additional insights into process performance gained when exergy analysis is considered in addition to energy analysis are discussed.

1. INTRODUCTION
In this study, energy and exergy analyses are used to assess the performance of a nuclear steam power plant. It is hoped that this examination, primarily because it includes exergy analysis, will yield new insights into the performance of nuclear steam power plants.

A complete analysis of the thermodynamic performance of a process generally requires the use of both energy and exergy analyses. Exergy analysis, because it accounts for losses due to internal consumptions and external wastes, is regarded by many to give more meaningful and illuminating results than energy analysis (1-6).

For nuclear technologies, exergy analysis can be particularly effective in identifying ways to improve the performance of existing operations, and designing and optimizing future plants. When cogeneration systems for power and heat are considered, exergy analysis should be used because, unlike energy analysis, exergy analysis weighs heat and work according to their usefulnesses (by assessing the "work potential equivalent" of the heat).

2. BACKGROUND
The particular plant considered in the present analysis is the Pickering Nuclear Generating Station. The station uses the CANDU (Canadian Deuterium Uranium) reactor concept. The process flowsheet for the plant is shown in Fig. 1. The letters identifying the streams in Fig. 1 are explained in Table 1. The main process data, drawn from Refs. 7 and 8, are summarized in Table 2. For convenience and to bring out important points in later discussions, the plant is separated into four sections.

2.1 The Steam Generation Section
In the Steam Generation section (Devices A, B, C and D in Fig. 1), heat is produced in a reactor and transferred via the Primary Heat Transport (PHT) loop to the boilers, where it is used to generate steam from preheated water. In each unit of the Pickering Generating Station, natural uranium, in the presence of a moderator, is fissioned to produce heat. 7724 kg/s of pressurized heavy water (D2O) flows in the PHT loop, which transfers heat from the reactor to the boilers. The D2O is heated from 249°C and 9.54 MPa to 293°C and 8.82 MPa in the nuclear reactor. 815 kg/s of steam (H2O) at 4.2 MPa and 251°C is produced in the boiler, and is transported through the Secondary Heat Transport loop. Spent fuel is removed from the reactor, and heat generated in the moderator is rejected.

2.2 The Power Production Section
Basicallly, in the Power Production section (Devices E, F, G, H and I in Fig. 1), the steam produced in the Steam Generation Section is passed through a series of turbine generators. The voltage of the electricity is adjusted in a transformer. Extraction steam from the turbines is used in the Preheating Section.

For nuclear technologies, exergy analysis can be particularly effective in identifying ways to improve the performance of existing operations, and designing and optimizing future plants. When cogeneration systems for power and heat are considered, exergy analysis should be used because, unlike energy analysis, exergy analysis weighs heat and work according to their usefulnesses (by assessing the "work potential equivalent" of the heat).

2.3 The Condensation Section
In the Condensation section (Device J in Fig. 1), cooling water condenses the steam exhausted from the turbines. The flow rate of the cooling water is adjusted so that a temperature rise of 11°C in the cooling water
is achieved across the condenser.

2.4 The Preheating Section

In the Preheating section (Devices K, L, M, N, O and P in Fig. 1), the temperature and pressure of the condensed steam are increased in a series of pumps and heat exchangers.

3. THEORY

Three fundamental principles are involved in energy and exergy analyses:

- Conservation of mass.
- Conservation of energy (the first law of thermodynamics).
- Non-conservation of entropy (the second law of thermodynamics). The entropy of an isolated system remains constant (when reversible processes occur in it), or increases (when irreversible processes occur in it).

For a control volume (Fig. 2) undergoing a steady-state process, with material, heat and work interactions occurring at discrete points on its surface, the expressions for the three principles respectively are

\[ 0 = \sum m_j \dot{h}_j \]  
\[ 0 = \sum (e + P\nu) m_j + \sum \dot{Q}_j + \sum \dot{W}_j \]  
\[ -\dot{s} = \sum s_j m_j + \sum \dot{Q}_j/T_j \]

where the summations are over all streams interacting with the control volume, and where

- \( \dot{m} \) : mass flow rate
- \( e \) : energy per unit mass crossing the control surface (including internal kinetic and potential energy)
- \( P \) : pressure
- \( \nu \) : specific volume
- \( s \) : entropy per unit mass
- \( T \) : temperature
- \( \dot{Q} \) : heat flow rate
- \( \dot{W} \) : work rate
- \( \dot{s} \) : rate at which entropy is created in the control volume.

Flows into the control volume are defined as positive, and out of the control volume as negative.

Exergy is defined as the maximum amount of work which can be produced by a stream of matter, heat or work as it comes to

equilibrium with an environment. The environment is defined by specifying the temperature $T_o$, pressure $P_o$ and chemical composition. The concept of the environment, and recommendations on selecting an appropriate reference environment for a specific problem, are discussed elsewhere (1-6).

Equations 2 and 3 can be used to derive the following steady-state "exergy balance:"

$$\sum Ex_m + \sum Ex_h + \sum Ex_w = Ex_c \quad (4)$$

where the summations are over all streams.

The exergy consumption rate in the control volume is given by

$$Ex_c = T_o \dot{S} \quad (5)$$

The exergy flow rates of work, heat and material streams respectively are:

$$\dot{Ex}_w = \dot{W} \quad (6)$$

$$\dot{Ex}_h = \dot{Q} \quad (7)$$

$$\dot{Ex}_m = (\dot{H} - \dot{H}_o) - T_o (\dot{S} - \dot{S}_o) + \sum N_i (\dot{u}_i - \dot{u}_i^o) \quad (8)$$

### TABLE 1 STREAM DATA

<table>
<thead>
<tr>
<th>Stream*</th>
<th>Flowrate** (kg/s)</th>
<th>Temperature (°C)</th>
<th>Pressure (N/m²)</th>
<th>Vapour Fraction</th>
<th>Energy (MW)</th>
<th>Exergy (MW)</th>
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* Stream identifiers beginning with S are material, Q are heat and P are power.

** All streams are H2O, except S1, S2, S3A, S4 and S5A which are D2O.
### TABLE 2 MAIN PROCESS DATA FOR ONE STEAM UNIT

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<tr>
<th>Section</th>
<th>Description</th>
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<tr>
<td>Steam Generation Section</td>
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<tr>
<td></td>
<td>Heavy Water mass flow rate</td>
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<td>D_2O temperature at reactor inlet</td>
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<td></td>
<td>D_2O temperature at reactor outlet</td>
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<td></td>
<td>System pressure at reactor outlet header</td>
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<td></td>
<td>Boilers</td>
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</tr>
<tr>
<td></td>
<td>Feed Water temperature</td>
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</tr>
<tr>
<td></td>
<td>Total evaporation rate</td>
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<tr>
<td></td>
<td>Steam temperature</td>
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<td>Steam pressure</td>
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<table>
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<th>Power Production Section</th>
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<td>Net power output</td>
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<table>
<thead>
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<td>Cooling water temperature rise</td>
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where
- ξ: dimensionless exergetic temperature
- \( \dot{H} \): \( \dot{m}(e \cdot PV) \)
- \( \dot{S} \): \( \dot{m}S \)
- \( \dot{H}_0 \): \( \dot{H} \) (evaluated at T. and P.)
- \( \dot{S}_0 \): \( \dot{S} \) (evaluated at T. and P.)
- \( \dot{n}_i \): molar flow rate of component i
- \( e_i \): chemical potential of component i at T. and P.
- \( \phi_m \): chemical potential of component i in the environment

and other symbols are as defined previously.

### ANALYSIS APPROACH

The plant was modelled and simulated using Aspen Plus, a state-of-the-art process-simulation computer code. Then, energy and exergy analyses were performed using a version of Aspen Plus which had previously been enhanced by the authors for complete and unified energy exergy analysis. The development of the enhanced version of Aspen Plus is described in Refs. 9-11. The enhanced code has been applied to coal-fired steam power plants (12), nuclear steam power plants (11-13), and production processes for hydrogen (9-11,14-18), methanol (9,11,19) and ammonia (9).

### 1.1 Assumptions

Several assumptions were used to simplify modelling:
- The turbines were assumed to have isentropic efficiencies of 80% and mechanical efficiencies of 95%.
- Heat losses from all components were neglected, except for the generators and transformers, which were each assumed to be 99% efficient.
- Heat losses from the generator and transformer are taken to occur at the temperature of the environment; consequently, zero exergy is associated with the heat.
- D_2O was modelled as H_2O.
- The net heat delivered from the entering fuel and exiting spent fuel was considered as the main energy input to the plant.
- The potential temperature of the heat produced in the nuclear fuel was assumed to be high enough that the quantities of energy and exergy of the heat could be considered equal.
- The supply and removal of fuel was assumed to be a steady-state process.

### 4.2 The Selected Environment Model

The environment model used is as follows: \( T_0 = 15°C \) and \( P_0 = 1 \) atm. An environment temperature of 15°C was used because that is the approximate mean temperature of the lake cooling water. An environment pressure of 1 atm was used because it is representative of the mean atmospheric pressure in which the plant operates. The exergy analysis results are independent of the choice of the chemical composition of the environment.

### 5. RESULTS

The simulation results (e.g., flows, temperatures, pressures, etc.) are summarized in Table 1 for the main process streams identified in Fig. 1. Detailed results are given in Ref. 12.

![Fig. 2. A control volume.](image)
5.2 Results of Energy and Exergy Analyses

Energy and exergy values for the streams identified in Fig. 1 are given in Table 1. Exergy-consumption values for the devices are listed, according to flowsheet sections, in Table 3. These data are presented diagrammatically in energy and exergy flowsheets (Fig. 3). The net energy and exergy flows and exergy consumptions are shown. The magnitude of the energy (or exergy) of a stream is indicated by the width of the flowsheet line representing the stream.

The data are summarized in an informative manner in the overall energy and exergy balances shown in Fig. 4. Inputs and outputs (as well as internal consumptions for exergy) are represented. Note that cooling water inputs, because they contain zero energy and exergy, are not shown on the left sides of the pie charts; and that the reactor is taken to be only the fission reactor itself, not the total PHT loop.

### TABLE 3 BREAKDOWN BY DEVICE OF EXERGY CONSUMPTIONS (IN MW)

<table>
<thead>
<tr>
<th>Section</th>
<th>Exergy Consumption (MW)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Steam Generation Section</td>
<td></td>
</tr>
<tr>
<td>Reactor</td>
<td>969.7</td>
</tr>
<tr>
<td>D.O-H.O Heat Exchanger</td>
<td>17.4</td>
</tr>
<tr>
<td>D.O Pump</td>
<td>1.1</td>
</tr>
<tr>
<td>Moderator Cooler</td>
<td>9.0</td>
</tr>
<tr>
<td><strong>Total</strong></td>
<td><strong>1027.2</strong></td>
</tr>
<tr>
<td>Power Production Section</td>
<td></td>
</tr>
<tr>
<td>H.P. Turbine</td>
<td>36.9</td>
</tr>
<tr>
<td>L.P. Turbines</td>
<td>79.7</td>
</tr>
<tr>
<td>Generator</td>
<td>5.5</td>
</tr>
<tr>
<td>Transformer</td>
<td>5.5</td>
</tr>
<tr>
<td>Steam Separator</td>
<td>0.2</td>
</tr>
<tr>
<td>Closed Steam Reheater</td>
<td>15.0</td>
</tr>
<tr>
<td><strong>Total</strong></td>
<td><strong>142.8</strong></td>
</tr>
<tr>
<td>Condensation Section</td>
<td></td>
</tr>
<tr>
<td>Condenser</td>
<td>24.7</td>
</tr>
<tr>
<td><strong>Total</strong></td>
<td><strong>24.7</strong></td>
</tr>
<tr>
<td>Preheat Section</td>
<td></td>
</tr>
<tr>
<td>Low-Pressure Heat Exchangers</td>
<td>1.6</td>
</tr>
<tr>
<td>Deaerating Heat Exchanger</td>
<td>1.8</td>
</tr>
<tr>
<td>High-Pressure Heat Exchangers</td>
<td>16.4</td>
</tr>
<tr>
<td>Hot Well Pumps</td>
<td>0.04</td>
</tr>
<tr>
<td>Heater Condensate Pumps</td>
<td>0.03</td>
</tr>
<tr>
<td>Boiler Feed Pumps</td>
<td>0.43</td>
</tr>
<tr>
<td><strong>Total</strong></td>
<td><strong>20.0</strong></td>
</tr>
<tr>
<td><strong>Total</strong></td>
<td><strong>1215.5</strong></td>
</tr>
</tbody>
</table>

Fig. 3a. Simplified flow diagram indicating net energy flows in MW. Sections of plant shown are Steam Generation (S.G.), Power Production (P.P.), Condensation (C), and Preheating (P.). Streams shown are power (P), heat input (Q) and heat rejected ($Q_r$).

Fig. 3b. Simplified flow diagram indicating net exergy flows and consumptions in MW. Exergy consumptions in devices are given by negative numbers, and are illustrated as shaded regions. Other details as in Fig. 3a.
6. DISCUSSION

6.1 Overall Process Efficiencies

Energy efficiency, $\eta$, and exergy efficiency, $\epsilon$, values were evaluated for the overall plant:

$$\eta = \frac{\dot{E}_{\text{product}} - \dot{E}_{\text{pumps}}}{\dot{E}_{\text{heat}}} = \frac{545 - 19}{1763} = 30\%$$

and

$$\epsilon = \frac{\dot{E}_{\text{product}} - \dot{E}_{\text{pumps}}}{\dot{E}_{\text{heat}}} = \frac{545 - 19}{1763} = 30\%$$

where $\dot{E}$ and $\dot{E}_{x}$ denote respectively flows of energy and exergy. The energy and exergy efficiencies are identical here because it was assumed in the analysis that the specific energy and specific exergy of uranium are equal. The energy efficiency of 30% calculated here compares well with the value of 29.5% reported elsewhere for the same plant (7).

Although the overall energy and exergy efficiencies were found to be identical, there were many subprocesses within the station for which the energy and exergy efficiencies differed markedly. Therefore, the location of the principal losses were indicated to be in different subprocesses, depending on whether an energy or exergy analysis had been used. Generally, it was shown (see Fig. 4) that the main losses occur due to internal consumptions (as exergy analysis indicates), not due to external emissions (as energy analysis indicates).

6.2 Examination of the Steam Generation Section

Substantial exergy consumptions occur in the Steam Generation section. Exergy consumptions in the nuclear reactor and the other devices in the PHN loop are responsible for

$$\left[ \frac{1027}{(1763 - (545 - 19))} \right] [100\%] = 83\%$$

of those in the plant.

The energy and exergy efficiencies were found to be:

$$\eta = \frac{1780 - 487}{1368} = 95\%$$

and

$$\epsilon = \frac{838 - 132}{1427} = 49\%$$
for the Steam Generation section. The Steam Generation section appears significantly more efficient on an energy basis, than it does on an exergy basis. Physically, this discrepancy implies that although 95% of the input energy is transferred to the preheated water, the energy is degraded as it is transferred. Energy analysis neglects such losses, whereas exergy analysis accounts for them.

Of the 1027 MW of exergy consumed in the PHT loop, 47 MW was consumed in the boiler, 9 MW in the moderator cooler, 1 MW in the heavy-water pump, and 970 MW in the reactor. The exergy consumptions in the reactor can be broken down further by considering the separate subprocesses occurring within it (Fig. 5):

- Heating of the moderator.
- Heating of the fuel pellets (to their maximum temperature of approximately 2000°C).
- Transferring the heat within the fuel pellets to the surface of the pellets (where the temperature is approximately 400°C).
- Transferring the heat from the surface of the fuel pellets to the cladding surface (at 304°C).
- Transferring the heat from the cladding surface to the preheated boiler feedwater to produce steam.

For convenience, it was assumed that all the heat responsible for heating the moderator was produced in the moderator.

Detailed analyses by Ontario Hydro of heat losses indicate that of the 90 MW lost to the moderator, only 82 MW is produced in it. Of the remaining 8 MW which ends up in the moderator, 2.6 MW is lost from the fuel channel to the moderator, and 6.1 MW is produced in other reactor components and then transferred to the moderator. The breakdown of the devices in which the 6.1 MW is produced is as follows: 1.1 MW in the shield, 0.1 MW in the dump tank, 2.4 MW in the calandria and 2.5 MW in the calandria tubes.

The step in which heat is generated by fissioning nuclear fuel (also shown for completeness in Fig. 5) is taken to be outside the boundary of the nuclear reactor considered in this study. (It was earlier assumed that the net heat delivered by the nuclear fuel is the main energy input to the nuclear station.) The energy and exergy efficiencies calculated could be significantly different if this step were considered. In this case, the energy and exergy associated with the fresh and spent nuclear fuel would be required. The question of what is the exergy of uranium is not yet resolved (6,20). Most researchers contend that the exergy of uranium is the same as its energy. Some contend that it depends on the technology being considered.

Since D₂O was modelled as H₂O, a species with no chemical exergy because it exists as a condensed phase in the environment, the chemical exergy of D₂O was neglected. A complete exergy analysis, however, should account for the chemical exergy of D₂O-containing streams. The chemical exergy of D₂O is discussed in the appendix. Neglecting the chemical exergy of D₂O does not significantly affect the exergy analysis.

Fig. 5. Breakdown of the inefficiencies by process in a nuclear reactor. Material flows are represented by solid lines, and heat flows by dashed lines. The heavy solid line encloses the part of the nuclear reactor considered in the present analysis. The approximate temperatures of heat streams are indicated. Exergy flow rates (in parentheses) and energy flow rates are indicated for streams, and exergy consumptions (negative values in parentheses) for processes. All values are in MW.
results because the D,O is contained in the closed PHT loop of the Steam Generation Section. Since D,O is used only as a medium to transfer thermal energy, it is only the physical exergy of D,O that is of interest. If, on the other hand, a heavy-water distillation plant was being considered, the chemical exergy would be significant, because in that case D,O would be the principal product.

### 6.3 Examination of the Condensation Section

Energy analysis indicates that almost all the losses are associated with the heat rejected by the condensers (see Fig. 4). Exergy analyses indicate that the condensers are not responsible for large losses. This discrepancy arises because heat is rejected by the condensers at temperatures very near that of the environment.

In general, the condensers are devices in which:

- a large quantity of energy enters (1125 MW), of which close to 100% is rejected, and
- a small quantity of exergy enters (44 MW), of which approximately 50% is rejected and 50% is internally consumed.

The characteristics of condensers can be seen more clearly by evaluating the "net station condenser heat (energy) rejection rate",

\[
R_{\text{energy}} = \frac{\text{heat rejected by condenser}}{\text{net power produced by station}}
\]

and comparing it to an analogous quantity, the "net station condenser exergy rejection rate",

\[
R_{\text{exergy}} = \frac{\text{exergy rejected by condenser}}{\text{net exergy produced by station}}
\]

For the nuclear steam power plant:

\[
R_{\text{energy}} = 1.107 \text{ MW} / (515 - 19) \text{ MW} = 2.10
\]

and

\[
R_{\text{exergy}} = 21 \text{ MW} / (545 - 19) \text{ MW} = 0.0399
\]

The \( R \) values indicate that the exergy rejected by the condensers is less than 1% of the net exergy produced, while the energy rejected is approximately 200% of the net energy produced.

### 6.4 Examination of Other Sections

In the Power production and Preheating Sections, energy losses were found to be very small (less than 10 MW total), and exergy losses were found to be moderately small (approximately 150 MW in the Power Production Section and 25 MW in the Preheating Section). The exergy losses are almost completely associated with internal consumptions.

### 7. CONCLUSIONS

Both energy and exergy analyses, because they provide different information about process performance, are useful tools for examining the performance of electrical generation processes. Tasks, such as design, optimization and synthesis of processes, as well as other endeavours involving decision making, can likely be better performed if the results of an exergy analysis, in addition to those of an energy analysis, are considered. For instance, other processes for utilizing nuclear energy (Ref. 21 discusses some possibilities) may be better analyzed if both energy and exergy analyses are performed.

In particular, it was shown for nuclear steam power plants that the greatest potential for improving efficiency is in the nuclear reactors, and that the heat rejected by the condensers, which is substantial in quantity but low in quality (i.e., at a temperature near to that of the environment), is for the most part not very desirable. Exergy analysis brought out some points that energy analysis did not. Also, for results that were brought out by both analysis techniques, the results were illustrated in a more intuitive way using exergy analysis than energy analysis. In the development of future nuclear technologies and cogeneration systems, exergy analysis should be applied.

### ACKNOWLEDGEMENTS

Financial support for this research was provided by a Link Foundation Energy Fellowship and the Ontario Ministry of Energy, and is gratefully acknowledged.

### NOMENCLATURE

- \( \dot{E} \): energy rate
- \( e \): specific energy
- \( \dot{E}_x \): exergy rate
- \( e_x \): specific exergy
- \( H \): enthalpy rate
- \( m \): mass flow rate
- \( N \): mole flow rate
- \( P \): pressure
- \( R \): universal gas constant
- \( S \): entropy rate
- \( s \): specific entropy
- \( T \): temperature
- \( y \): specific volume
- \( W \): work rate
- \( x \): concentration of D,O
APPENDIX

ON THE CHEMICAL EXERGY OF HEAVY WATER

D,O has chemical exergy due to its purity with respect to D2O found in the environment. The chemical exergy of pure D,O is the minimum amount of work required to produce a unit of D,O from the environment.
Using equations for ideal solutions (22), the specific chemical exergy of D$_2$O can be evaluated at $T_o$ as follows:

$$ex^{ch} = R \, T_o \left[ x_o \ln \left( \frac{x_o}{x''} \right) + (1-x_o) \ln \left( \frac{(1-x_o)}{(1-x'')} \right) \right]$$

(11)

where $R$ is the universal gas constant (8.314 J/mol K), $T_o$ is the temperature of the environment, $x_o$ the mole fraction of D$_2$O in a stream of D$_2$O at $T_o$, and $x''$ the mole fraction of D$_2$O in the environment.

By noting that reactor-grade D$_2$O is 99.75% pure and that the concentration of D$_2$O in environmental water is 1 mole D$_2$O to 7000 moles H$_2$O (23), the specific chemical exergy of reactor grade D$_2$O at 298 K can be evaluated:

$$ex^{ch} = (8.314 \, J/mol \, K) \, (298 \, K)$$

$$[.9975 \ln (.9975/.000143) + (1-.9975) \ln ((1-.9975)/(1-.000143))]$$

$$= 21,835 \, J/mol$$
Ontario Hydro in investigating methods to reduce cost and construction time of nuclear plants. This study is essential to be able to respond to demands for power into the 21st century. As part of this study, Ontario Hydro is learning from its counterparts world-wide. The authors visited Japan in 1985 and obtained information on principles and practices of design and construction in that country. These and their application to the local scene are discussed.

THE NEED TO REDUCE CAPITAL COSTS

When the fourth unit of Darlington entered commercial service in 1987, Ontario Hydro will have completed a nuclear building program of 14 units, from 70 reactors at three sites. At present the utility is involved in a far reaching study of ways and means to satisfy the power demands of Ontario into the 21st century. One of the future options is clearly nuclear, which in 1985 provided over 40% of Ontario Hydro's power safely, and with minimal effects on the environment. However, on the important requirements of any future power source will be the ability to construct it quickly and at acceptable capital cost. Uncertainties in the demand for power over the 10 to 15 year period required to design and construct a four unit nuclear plant threaten the choice of this option. Consequently, a reduction in construction time can provide valuable flexibility to power planners and save interest charges and administration costs at the plant site.

Ontario Hydro Studies

Ontario Hydro has embarked on a nuclear cost reduction study within the broad framework of corporation-wide studies of Supply and Demand. The objective of the study is to investigate methods whereby improvements can be made in construction schedules and savings in the overall cost of designing and building new plants. New large multi-unit stations and add-on units to existing plants are under study.

Preliminary work has centered on several technological areas that promise benefits in cost reduction. These are: computer-aided methods for design, drawing production and modelling the plant. The integration of cost and schedule systems, improved construction techniques and modular prebuilt plant systems to save construction time.

The necessity to pre-design and complete the safety analysis (to facilitate early licensing) has also been recognized as an important element in the strategy of reducing costs and increasing flexibility in commitment of major projects. One of the targets of the study is to reduce direct costs, i.e., system hardware costs, by 10%. As an example of the magnitude of the savings possible by using such strategies, it has been calculated that possible savings in construction time could reduce interest charges on a 4 unit station by as much as $1.5 billion in dollars of the year.

Consultation

Ontario Hydro is consulting with other utilities, consultants and manufacturers in this area of cost and schedule reduction. To this end, the authors visited Japan in late 1985 and saw two manufacturers, four utilities and five reactors under construction. They obtained information on the current principles and practices of design and construction in that country. The principles are described below and are illustrated with some methods of interest in Canada. The application of these methods in Ontario is also discussed.

THE JAPANESE NUCLEAR SCENE

Organization of the Nuclear Industry.

The nuclear industry is at a third stage of evolution. In the first, plant designs were imported from the USA; then followed a period of domestic technological improvements. Finally in the third stage, designs are completely produced in Japan.

In its present form, the industry consists of 8 nuclear utilities and 3 large plant designer/constructors, Mitsubishi, Toshiba and Hitachi. They design and build the nuclear steam supply system, balance of plant and the turbine/generator. The civil works (which in Japan is limited to harbours, foundations and conventional buildings) are contracted directly by the utility. However, as the former is in the largest in terms of money and schedule importance, the plant designer dominates.

Improvements and Standardization Program
In 1965 the combined capacity for all Japanese nuclear plants was about 3 GBtu. Capacity factors had been falling since 1960, despite the fact that the nuclear power plants were built to be operated at a capacity factor of 40%. Utilities and the major nuclear plant manufacturers led by the Ministry of International Trade and Industry of the central government set up a national improvement and standardization program to rectify matters.

The result was a 5-year, 4-stage program. The first stage was concerned with practical matters to improve capacity factors, thermal efficiency, and efficiency of maintenance. The second stage continued these objectives and added reliability and reduced construction time. The third stage was concerned with new issues such as reactor design, construction, inspection, and design. In particular, methods to reduce construction time, discussed below, have been followed successfully. The fourth stage of the program concerns the advanced model PWR and PBWR plant. These lie in the future.

Then three-stage program, focused on blending government, utilities, manufacturers, and expert opinion only off. The average capacity factor for all Japanese nuclear plants increased steadily to over 70% by the end of 1984. This notwithstanding, a 5-fold increase in total generation capacity. In addition, as indicated in Table 1, construction time decreased considerably. Eight PWR and PBWR units were completed in 1984, compared with only four units in 1977.

<table>
<thead>
<tr>
<th>Year</th>
<th>Units Completed</th>
</tr>
</thead>
<tbody>
<tr>
<td>1977</td>
<td>4</td>
</tr>
<tr>
<td>1978</td>
<td>4</td>
</tr>
<tr>
<td>1979</td>
<td>2</td>
</tr>
<tr>
<td>1980</td>
<td>2</td>
</tr>
</tbody>
</table>

Table 1: Improvement/Standardization Program: Effects on Construction Time (1977-1984)

The drive for shorter construction time has meant that many construction activities now take place in the factory rather than on site. This improves quality and has several other desirable features, not the least of which is the absolute necessity to preplan the whole construction sequence throughly.

The negative side is the extra costs involved in the plant design and construction.

**INNOVATIVE METHODS**

Pre-Construction Planning:

Conceptual design of a new plant typically starts about 5 years prior to the commencement of construction (referred to here as 5-year planning). During this period extensive design work is undertaken by the design team in areas of construction methods, techniques, work scheduling, etc. The plant designer is in an excellent position to do this effectively. Each of the 4 main plant design companies is sufficiently large so as to manufacture all major equipment themselves. Unlike their counterparts in other countries, they have the advantage of being able to specify their own equipment designs and selection in advance of ordering the equipment. Not only is the plant designer familiar with the design and research and development of the equipment, but system and safety analysts can commence the detailed work to confirm the design and prepare the licensing documents.

Equipment selection takes place at least 3 years before the contracts are issued, which means that the equipment is not released until construction is complete (at 4 years).

Extensive analysis of previous construction projects convinces Japanese utilities and their plant design of the benefits of reducing the interval between front-end and construction design stages. This is achieved by a reduction in the number of iterations between the drawing review and the next design stages. Present practice is to complete between 77 and 80% of drawings before construction begins. Present practice is to complete between 77 and 80% of drawings before construction begins.

All plant designers use a mix of models, manual methods and computer aided design (CAD) packages. There are 3 main types of design models that are used: models of equipment, models of facilities, and models of the process flow. The use of CAD software is becoming more prevalent, as well as layout models.

From a study of the models, changes are often made to the design of the plant, or new tools or tags etc. are devised to speed construction. Plant designers seem satisfied with this mix of physical and computer models of the plant. They do not envisage much change in the near future, and have no plans to phase out physical models.
Reduction in construction time is a result of 4 concepts: (1) reduced site work, (2) parallel work on critical activities and (3) improved efficiency of work at the site.

1. Reduced site work

Pre-installation: Reactor pressure vessel internals of some HWR's are installed in the factory, analogous to producing a CANUS calandria.

Modules: As many as 40 factory built pipe and equipment modules weighing up to 400 are employed in a plant, reducing site installation, welding and testing. Modules are designed from the plant design models. Usually modules are comprised of process equipment, piping, supports and steel frames. Electric and control cables are fitted later. Modules are installed at an early stage of construction, often in parallel with other concrete work. Figure 1 shows a typical module being lowered into position. Note that the roof has still to be poured.

![Fig. 1 Process system module located in partly completed reactor building](image)

Table 2 shows typical welding point data for modules in 2 HWRs. The total number of welds is increased by 7% by the use of modules, but the number of field welds which are more difficult to make and inspect are reduced by 45%.

<table>
<thead>
<tr>
<th>Number of modules</th>
<th>Number of modular</th>
<th>Number of non-modular</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Shop</td>
<td>Site</td>
</tr>
<tr>
<td>Takahama 1</td>
<td>38</td>
<td>4,000</td>
</tr>
<tr>
<td>Takahama 4</td>
<td>20</td>
<td>2,750</td>
</tr>
<tr>
<td>Total</td>
<td>58</td>
<td>6,250</td>
</tr>
</tbody>
</table>

**TABLE 2**: Reduction in site welds due to adoption of modules

Prefabricated steel and concrete framework: Up to 60% of total is prefabricated on site. Figure 2 shows typical prefabricated elements such as columns, walls and beams. Some beam elements used in the turbine hall weigh 40t. Permanent concrete framework is another type of prefabricated element. Wall panels 2 by 5.5 m, only 120 mm thick are attached to structural steel building frames, rendering the building weather tight. These panels form the outer (permanent) wall surface. The inner framework is conventional wood or steel and is erected later allowing the concrete to be poured at the construction's convenience.

![Fig. 2 Typical prefabricated reinforced concrete elements](image)

Conventional steel frame buildings or sections of buildings are widely used for the framework of structures clad in concrete. They can be erected rapidly and made weather tight if so required. They can later be fitted with modular modules and become part of the concrete structure.
2. Parallel work

The Deck Plate Method: Scaffolding to support floor slabs is eliminated by pouring the floor on steel deck plate supports on permanent or temporary steel beams as shown in Figure 3. This ensures a free work space on the lower floor for the installation of equipment during the construction of the building. The method is used for up to 20% of the reactor building floor area, and is credited with reducing construction time by 1 to 2 months, as shown in Figure 4.

![FIG. #3 Deck plate method of construction](image)

**FIG. #3 Deck plate method of construction**

![FIG. #4 Construction time saved by use of deck plate method](image)

**FIG. #4 Construction time saved by use of deck plate method**

Equipment installed during building construction: Space is often severely limited on Japanese construction sites. Some sites are on relatively small areas between hills and sea. The elevation difference is often used to advantage by the construction of ramps from the hillside connecting to platforms which penetrate the space between the various buildings.

Figure 5 shows the system of platforms and access ramps for a twin unit PWR plant. These platforms are positioned relative to the radius of tower cranes so that equipment can be trucked in and lifted into position by crane. As the building height increases during construction, the platform height is adjusted to maintain access. Similarly, the ramps are built up to maintain access. The platforms carry trucks loaded with material, equipment and in some cases, concrete. Figure 6 shows access ramp and platform at a PWR site.

![FIG. #5 System of ramps and platforms provide access during construction](image)

**FIG. #5 System of ramps and platforms provide access during construction**

![FIG. #6 Platform access at a PWR site](image)

**FIG. #6 Platform access at a PWR site**

1. **Increased Site Work Efficiency:**

Automatic Welding Machines: Savings in manpower and improved quality have been achieved for Class I, II and III welds by the use of automatic welding machines for pipe and vessels.

"Reduction in rework": While much rebar is prefabricated and assembled on site, there is, of course, a need to locally position...
Rebar for walls, beams, floors, etc. Unmanned robot manipulators are used for lifting and positioning rebar. Some machines are equipped with adaptive learning systems. These enable the operator to take the machine through a typical routine once and have it repetitively perform the routine tasks; positioning rebars in successive locations. The robots are of two types; small tracked machines for placing floor rebar and larger, crane-like machines for walls as shown in Figure 7. Robots are used to save manpower and reduce the risk of injury to workers, in addition to increasing efficiency.

Temporary load carrying scaffolding was erected adjacent to the reactor building to facilitate lifting the vessels into position. However erection and dismantling the scaffolding was a critical path activity whereas erecting a special purpose crane was an independent activity hence its recent adoption. Figure 8 shows a large derrick crane lifting a 1100 t BWR reactor vessel into the reactor building. Generally, improved crane layout is credited with 1 to 3 months reduction in construction time.

FIG. #8 Special purpose crane lifts 1100t pressure vessel at BWR site.

All the above methods, and others not discussed here are credited in reducing total construction time from 77.6 months to 55 at a BWR site.

APPLICATION TO CANDU CONSTRUCTION

The principles of reduced site work, parallel work and improved efficiency at the work site are all applicable to CANDU's. Many innovative methods are in use already, however, several of the methods above have considerable potential for reducing construction time when applied in a form suitable to local conditions.

Among the methods under continuing study by Ontario Hydro are:

1. Reduction in structural dependency between substructure walls and floors to allow floors to be poured as secondary pours.

2. Reduce the number of pours in walls and slabs.

3. Use precast slabs in room roof so that the structure can be complete prior to boiler installation.
4. Reduce rebar and formwork quantities.
5. Pre-assemble components on the calandria such as shield tank extension, reactivity mechanism deck, etc.
6. Prefabricate large system modules

These, and other methods will keep the CANDU concept viable for the future.

ACKNOWLEDGEMENT

The authors wish to thank their colleagues in the Electric Power Development Co., Tokyo Electric Power Co., Chubu Electric Power Co., Hokkaido Electric Power Co., Mitsubishi Heavy Industries Ltd. and Toshiba Corp., all of Japan for their information and assistance.
The necessity for nuclear power: the oxygen-CO₂ balance

John B. Hamburg

Abstract

The oxygen and carbon dioxide content of the air are critical to the survival of most life on earth. We are now burning huge amounts of fossil fuel to obtain heat and energy, depleting our oxygen supply and increasing the carbon dioxide in the air. We can get all the energy we need from nuclear power, safely, cheaply, and cleanly, and nuclear power consumes no oxygen and makes no carbon dioxide and causes no acid rain. The problem of nuclear waste and by-products have been solved.

History

Two billion years ago the earth had a reducing atmosphere. Free oxygen was absent and carbon dioxide was abundant. The only living creatures were one-celled creatures, including blue-green algae, the first living things capable of photosynthesis. That is the breakdown of carbon dioxide, the carbon going into various organic compounds and the oxygen being released into the air. Over the next billion years (more or less) much carbon dioxide broke down, the oxygen content of the air increased to about 2%, by weight, 30.0% by volume. The carbon, after much alteration became our coal deposits and oil deposits and probably most of our natural gas deposits. The carbon dioxide content of the air dropped to about 100 parts per million.

Soon, in geological terms, after oxygen became abundant, oxygen breathing animals appeared and increased through the ages to the size of blue whales and to the intelligence of mankind.

There is considerable evidence that the oxygen and carbon dioxide content of the air has remained quite constant over the last 300 million years. These proportions seem to be self-regulating in nature and remain constant until the industrial revolution became widespread.

Industrial Revolution

Free oxygen, the kind we breathe all the time, remains in the air because gravity is strong enough and the temperature too low so that very little escapes into interplanetary space. Oxygen now makes up about 21.2% of our atmosphere, a total of about 1.97 X 10²⁴ kilograms. This sounds like a lot, but is only one fifth of one part per million of the total weight of the earth, 5.98 X 10²⁴ kilograms.

Oxygen in chemical combination is fairly abundant on this planet. Water is about 89% oxygen and the oceans alone contain about 324 cubic miles (810 cubic kilometers) of water. The crust of the earth, the top 10 miles (16 kilometers) of solids, is about 46.6% oxygen. Thus there is about 8,000 times more oxygen in the crust of the earth than in the atmosphere.

The oceans and lakes have about 2.6% of oxygen in solution (not chemical combination). This seems quite small, but it is enough to keep the fish going.

Altogether our planet has a good supply of oxygen, which is a rare and precious condition since the universe is 92% hydrogen, 7% helium, leaving only 1% for the remaining 90 elements, including oxygen, the most abundant element in the crust of the earth and iron, which most experts believe is the most common element in the core of the earth.

Our present situation in the oxygen-carbon dioxide cycle is still good. Our atmosphere contains 21.2% oxygen, which is enough for ordinary breathing in a healthy person, and for most of our combustion to heat our homes, cook our food, and run our industries. We have reserves of minable coal estimated at about 4 trillion tons worldwide. We have oil reserves of billions of barrels and much natural gas. We have enormous deposits of oil shales and tar sands from which we can recover oil, though at great cost.

The crisis of the precipice

But we are on the brink of the precipice. The carbon dioxide content of the air is going up about one part per million per year. This means that we are adding about 5 X 10¹² more metric tons of CO₂ to the atmosphere per year. There is reduced by photosynthesis and is absorbed into the oceans. The world now burns about 3 billion tons of coal a year and by this process produces about 9 billion tons of CO₂ a year. The world burns about 66 million barrels of oil per day and that produces about 13 billion tons of CO₂ a year. We also burn huge quantities of natural gas and coal, and this produces more CO₂.

The respiration of all oxygen-breathing animals from termites to humans and decay of organic matter adds to this total.

We mine the best and most easily recovered coal first, thus the price of coal goes up each year. Fifty years ago Alaska did not bother with any natural gas, costing more than $10 per thousand cubic feet, now we drill to 30,000 feet to prove reserves that will cost more than $100 a thousand cubic feet.

So you see we are on the brink of disaster. Man is destroying in a few hundred years that nature built over a billion years. The situation regarding oxygen, CO₂, coal, oil, forests, and pollution are all alarming

The forests

Fire wood from the forests is the main source of cooking fuel in tropical countries. About 11 million hectares of prime woodland in Asia, Africa, and Latin America per year are cut and burned to cook the food of the poor. The removal of these trees causes soil erosion and desertification and loss of the habitat of many animals and plants. Very little of the area is replanted. This loss of trees also affects the oxygen, CO₂ balance as explained below.

The breakdown of CO₂ takes place in living plants, chiefly the tropical rain forests and the top hundred feet of the oceans and lakes. But we are killing our oceans with pollution, and chopping down our forests.
Our students of forestry tell us that half the rain forests of the far East from Burma to the Philippines have disappeared to make room for rubber, palm oil, and coconut plantations and other crops. The Congo rain forests we are slaughtering with slash and burn farming mostly by cutting thousands of acres for fire wood and other mismanagement.

The rain forests of the Amazon produce more oxygen than any other source, according to Dr. George B. Cashell, who has spent most of his adult life studying pollution, the oxygen-CO₂ cycle and related problems. (2) The people of Brazil are rapidly destroying the rain forests to open the area for farming, mining and other activities, with disastrous effect on the oxygen supply. These statements are not intended as criticism, but just to show the situation. In Canada, considering our dismal record in forestry management, (3) we cannot afford to criticize others.

(4) We desperately need an alternative source of heat for millions of homes in underdeveloped countries.

THE NUCLEAR POWER PLANT

We have a cure for these dismal situations, nuclear power, that can supply the world with all the power it needs and do it cheaply, cleanly and safely. It burns no coal or oil or gas or wood. It uses no oxygen and produces no carbon dioxide or acid rain. It destroys no forests and causes no deserts.

This means that every nuclear power plant anywhere in the world is a sound investment ecologically, economically.

This means that Ontario should speed up completion of Darlington and phase out coal and oil plants. All other provinces should seriously study construction of nuclear plants in place of coal and oil burning plants.

Most people here favour the Candu reactor as it can work on natural uranium which means that expensive uranium enrichment plants are not needed.

A few years ago an American utility suggested that Canada build nuclear reactors and sell the power to the U.S.A. cheaper because it causes no acid rain over Canada. I think we should sell the power cheaper because it causes no acid rain over U.S.A.

A POSSIBLE USE

The water supply to many areas of the earth is a major concern in these times of widespread famine and desertification. The Green Revolution depends on two things, water and fertilizers, and power is required for both. Nuclear reactors can supply this power.

The continents of the earth receive each year about 11,000 cubic kilometers of rain. (9) Many years ago when I was working in irrigated orchards in B.C. water was said by the "per acre root" and 11,000 cubic kilometers is about 2.83 X 10¹⁰ acre feet. The earth has about 2.5 billion acres under crop. The rain water if evenly distributed over the crop land would give about 20 feet (6 metres) of water which is much more than it is needed for any crop. But unfortunately much of the rain falls at the wrong time or in the wrong place for growing crops. To use this water to need power, pumps and pipes.

I estimate that a nuclear plant the size of Bruce could supply power to pump from the Congo River, one foot of water per year on one thousand square miles at the south edge of the Sahara desert. This much water is enough to grow crops and would stop the southward spread of the desert. I even imagine the ecological sound farming and forestry practice. This plant could also supply power to hot plates in thousands of homes, thus saving many acres of forest now cut down for fire wood each year. The cost would be about 15 billion dollars for the nuclear plant and about 15 billion for the pumps, pipes, distribution systems, etc. To keep costs in half proportion, 30 billion is less than the World Coal Study estimates would be needed in capital expenditure to take coal from North America to Europe. This 30 billion is less than the world now spends per month on armaments. This remark is not intended as anti-arms propaganda, but most of us realize that it would cost us more if, through lack of armaments, we were taken over by a harsh dictator. Ask the Bolivians, Lithuanians and Estonians.

Another system pumping from the Amazon to North East Brazil could be equally beneficial, and the same applies to other places.

Another billion dollars in a cheap price to pay to save a good part of a continent from becoming desert.

OBJECTIONS

Some people speak against nuclear reactors, let us deal with some of the objections raised.

Anti-nuclear groups say there is no safe way to dispose of radioactive substances in spent fuel rods. This problem was solved many years ago. See report E77-6 by N.B. Aiken, J.H. Burton and F.K. Harro of Energy Mines and Resources, Canada. (10) This report recommends putting the radioactive materials in tunnels 1,000 to 1,000 feet underground in stable granite or similar rock. The Precambrian Shield in Canada contains some of the oldest and most stable rock formations on earth and in Ontario and Maitika there are over 30 huge stocks of granite or basaltic rock, called "plutons", that have not had any geologic activity in the last billion years, and there is good evidence to believe it will have no activity in the next billion years. They are ideal places to dispose of radioactive waste. The worst possible scenario would be a long glacial period which could scrape up to 70 feet (20 metres) of the rock over leaving undisturbed anything buried 1,000 feet (100 metres) below the present surface. Due to lack of firm action from our government, Canada has not yet started such a project. The Chinese, who have in their territory large masses of granite rock are starting work and propose to take in nuclear waste from other countries as well as their own for a price. The Americans are studying sites in their small areas of Precambrian rock. Canada will probably miss out on this chance to have a thriving business with a nice profit, and perhaps the screw goes to the world. Information should be permitted to put radioactive waste in the ocean.

Anti-nuclear people say that the Candu reactor produces plutonium which they associate with the atom bomb. There are more than 15 isotopes of plutonium of which the Candu produces 3 or 4 in appreciable quantities. Each isotope has a different half life, varying from a few minutes to lives of millions of years and different physical properties, but the same chemical properties. As far as I know, and I have spent much time studying the matter, the only
isotope of plutonium used in the atom bomb is plutonium 239. It is relatively easy to separate plutonium from other elements by chemical means. All one needs is a chemical laboratory with heavy shielding and remote control costing probably a few tens of millions of dollars, and a crew of good chemists and chemical technologists. But separating the different isotopes is more difficult and expensive.

Anti-nuclear people say that the Candu reactor produces tritium which can be used in hydrogen bombs. Both USA and USSR have more than enough hydrogen bombs without buying tritium from Canada. Actually tritium is used mostly on lights that glow for 10 years without power or attention and for research of many kinds. I am told that each hydrogen bomb contains only 1 to 5 grains of tritium, and that the U.S.A. produces 4 or 5 kilograms per year.

Anti-nuclear people say that some people in most towns “don’t want” nuclear plants or disposal facilities in their back yard. I have no patience with people who think that if they shout “I don’t want”, the whole world should follow their wishes. The government of Canada acquired thousands of acres of land near Toronto and Montreal to build airports. One was never built and one became a “white elephant”!

Anti-nuclear people say that nuclear plants are expensive and usually cost more than the estimate made when they are started. There are a number of reasons for this, the most important being changes made after the work starts. (11) In many well run projects in private industry, at a certain stage in the planning, the project manager says, “This is the way we are going to build it. No matter what ideas anyone comes up with, we make no changes.” Canada has now had enough experience so that we can reach a design that we can accept as final. The owner should then contract practically everything by competitive bidding on the understanding that there will be no changes and no extras. Inspectors should have power to withhold payment on unsatisfactory work. Inspectors should be strict on essential items, but reasonable and fair. No knits pickers or bean counters.

New to the understanding that there will be no changes and no extras, Inspectors should have power to withhold payment on unsatisfactory work. Inspectors should be strict on essential items, but reasonable and fair. No knits pickers or bean counters. The French, who hold the record in nuclear plant construction, having commissioned 13 plants in 11 months, use this method. See the talk by L-Timbriel at the annual conference of the Canadian Nuclear Society, 2 June 1982. (12) The paper was given in English, but is reported in the proceedings in French. One can obtain a translation for 10 to 20 cents a word.

Canada has a commission that has the job of setting standards for the nuclear industry. As their job depends on it, they keep adding new regulations, some of which verge on the ridiculous, and add millions to the cost of construction. An example is the requirement that nuclear plants should be designed for an earthquake of magnitude 7 on the Richter scale even in a region where earthquakes are unknown. (13) We should restrain this commission.

CONCLUSION

A few concluding remarks are in order:

1. Coal, oil and natural gas are too precious to use to boil water to make steam for generation of electricity, when we can make steam more safely, cleanly and cheaply using uranium.

2. We should save our coal smelting and coal based chemicals. We should save our oil for petrochemicals and for small motors where nuclear power is not feasible.

3. With advances in battery technology and hydrogen technology, we can expect much wider use of both of them, thus increasing the demand for electricity to charge batteries and make hydrogen by electrolysis of water.

4. Developing countries need nuclear power plants for industry, cooking, and lighting, thus saving the forests and increasing the standard of living and education.

5. Alternative sources of energy, such as wind and solar, are fine for small uses, but are not much use to power a steel plant or multifamily housing or pumping station or a city like London or even Toronto.

6. Nuclear power makes no carbon dioxide or acid rain and uses no fossil fuel or wood or oxygen, all of which are in limited supply and irreplaceable.

7. Nuclear plants can produce cobalt 60, which emits gamma rays. These can be used to kill molds, insects and other things that destroy food. Proper use of gamma rays can greatly increase the food supply, without adding an acre to our cropland or a drop of water or a pound of fertilizer to our farms.

8. Most important of all, the free oxygen in our atmosphere is the most important possession of the inhabitants of this earth and we should take care of it, by obtaining power from nuclear plants as these do not use oxygen or produce carbon dioxide or acid rain.

REFERENCES


(2) Any textbook in geochemistry will give this information.

(3) Ibid.


(6) Many Authors, “Tropical Forests, a Call for Action”, World Resource Institute, Washington, D.C.

(7) WOODFIELD, GEORGE M., as (5).


(9) POSTELL, SANDRA, Article in “Natural History” April 1995.

(10) AITKEN, A.M., HARRISON, J.M., HAME, F.M.


(13) Personal communication from a staff member at Bruce.
ABSTRACT
The nuclear industry's principal concern with the news media must be to ensure that media representatives have ready and timely access to information sources. Candour, accuracy and timeliness will establish and maintain credibility and, importantly, result in better informed journalists. Response to stories/broadcasts should be limited to correction of gross errors or misrepresentations. The stylistic conventions of traditional scholarly discourse cannot be applied to the mass media. All other things being equal, general public acceptance of nuclear energy rests more on continuing safe and economic performance than anything else. Of more significance is the future—post-Darlington, in the case of Ontario. Those with input to energy policy formulation need better access to accurate information on the technical, environmental and economic advantages and limitations of nuclear energy systems. Without such information there is the danger that decisions will be made on inappropriate bases, with potentially serious long-term economic and environmental consequences.

INTRODUCTION
It is important to point out at the outset that the title for this presentation was selected in early January of this year and should not be construed as any direct reference to the events at Chernobyl. In fact the title is a slightly mangled quotation from Hamlet and was chosen to suggest that while the news media and the general public may be frightened by the "false fire" of misconceptions about nuclear energy, the nuclear energy community has certainly demonstrated fear of the "false fire" of the news media.

THE ROLE OF THE JOURNALIST
Rational consideration of the relationship between the nuclear industry and the news media has been (and is) impeded by a basic misunderstanding about the primary objective of any journalist. Much discussion about the news media, by journalists as well as their critics, centres on "getting the facts", "objective reporting" or "maintaining balanced reporting". But, however worthy these concepts may be, they are secondary to the principal objective of getting the attention of an audience.

Consider William Shakespeare. Shakespeare is one of the most influential figures on the world's cultural landscape. But also he was, quite simply, the most successful commercial playwright of his time, not because he had the scholarly imprimatur of the Seventeenth Century equivalent of the Canada Council, but because he knew how to fill the theatres. His plays gave his audiences what they wanted—a rich mixture of graphic sex and violence, villainy, romance and ribald humour—elements which have been more or less successfully concealed from generations of reluctant schoolchildren ever since.

Similarly the journalist must be able to entertain—to capture the audience's attention—attention for which many are in competition. The inevitable result is a dramatic and impressionistic approach to news reporting and, to a slightly lesser extent, to "editorial" or "documentary" items. It's important to note here that this approach informs all topics covered by the news media—not just nuclear energy.

Many people in the nuclear industry (this writer included) can list many examples of what they would describe as superficial, unfair or downright misleading treatment of their area of endeavour by the news media and can point to a seemingly endless list of gross technical errors in news reports, documentaries or editorials. But nuclear energy is certainly not unique in this respect. In the area of military aviation—the issue of the safety history of the CF-104 for example—it could be argued that the Canadian news media have been inaccurate to the point of being seriously misleading. The situation is similar in the case of non-technical issues. It is highly probable that both the medical profession and Ontario provincial politicians would be able to cite many examples of "inaccurate", "misleading" or "biased" coverage of the current extra-billing issue. When someone describes a newspaper article as "inaccurate" or "misleading" or "biased" it is quite probable the case that the person finds the story disagreeable on grounds other than those of absolute accuracy, mendacity or prejudice. It is not unduly cynical to suggest that a member of the Canadian Nuclear Society, upon reading an editorial strongly endorsing the CANNU system, might well forgive some technical inaccuracies in the piece.

To give things a little more perspective, it's necessary to remember that as specialists in one particular area of endeavour we regard mass media treatment of that endeavour from a specialist viewpoint to which the newspaper editor or TV producer cannot be expected to cater—any more than the specialist viewpoints of lepidopterists, aeronautical engineers or Shakespearian scholars.
may be catered to. The news media form, as Robertson Davies suggests in *Leaven of Malice*, a barber's chair which must fit all bottoms. It is interesting to consider how many members of the Canadian Nuclear Society would read political reports in the newspapers if these reports were limited to unedited Hansard transcripts and the actual text of proposed legislation.

The role of the journalist has, in the post-Watergate era, been described as developing to include the "Investigative" function and there has been considerable attention paid to "the new journalism". However, it is possible to argue that this is by no means "new" at all. Readers of the novels of Anthony Trollope will be familiar with the ubiquitous (and somewhat unsavoury) Mr. Quiritus Slide and the activities of that influential newspaper the Jupiter. It could be argued that the latter organ's activities, for example in *The Warden*, are indistinguishable from those of the twentieth Century Washington Post, although it must be emphasized that there is no intention to attribute to modern journalists the motives that impel Trollope's journalists.

The role of the journalist is to entertain and inform an audience - to inform by entertaining, attracting attention and stimulating the imagination. Where poetry and drama hold a mirror up to nature, journalism holds a mirror up to contemporary society and contemporary society's preoccupations and perceptions. It is important to emphasize that this is an honorable tradition as the works of two nineteenth Century journalists in particular - Thomas Carlyle and Charles Dickens - should remind us.

But it is also important to remember that as the demarcation lines between the journalist, essayist and novelist are blurred at best, the reporting of "information" in the news media will inevitably be a quite different activity to the reporting of information in the scholarly and scientific communities.

**JOURNALISTS AND "NEWS"**

Quite apart from the requirements imposed on the activity by the role of the journalist, we must remember the requirements imposed by the nature of the medium - be it print or electronic. The time and space constraints inherent to the business virtually ensure that "news" reporting will not provide the scope for the qualification, modification, analysis and review that is inherent to traditional scholarly discourse. A newspaper reporter has usually less than a thousand words in which to tell a story and a few hours (at most) in which to put those words together. For the television reporter the limits - both in time and depth - are even more rigorous. What is to be presented will inevitably be presented in broad brush-strokes. The journalist must produce a billboard because there isn't the time to produce (nor the audience for) a blueprint.

Remembering that the journalist must attract the attention of the audience, the billboard must be splashy enough to catch and hold the eye. There must exist, for example, the drama of confrontation. A statement by a government minister is seldom reported in the news media without coverage of at least one representative of an opposition party. And inevitably, a nuclear industry spokesperson's comments will rarely appear without the counterpoint of a representative from a nuclear opposition group. Journalism (as drama) thrives on conflict and to deplore this fact and its implications is as foolish as to deplore the law of gravity. Though it is fair to remind ourselves - and journalists - that just because there are two opposing views on a topic that does not mean that the truth of the matter lies midway between the two extremes.

The definition of "news" is not well nailed down, a fact which may account for many people in the nuclear industry voicing complaints to the effect that what they call "negative" stories about nuclear energy in Canada seldom include reference to the undoubted scientific and engineering achievements exemplified by the CANU system. A moment's reflection should be enough to see that this simply isn't "news". That the man who invented the cuckoo clock is dead is good, but it is not news. News coverage of the G-16 pressure tube failure at Pickering Unit 2 did not include any reference to the 389 tubes in the unit which had not failed.

In summary, if a journalist is to tell a story, he or she must attract and retain an audience's attention and must appeal to as wide a segment of the potential audience as possible. Additionally the time and space constraints under which the journalist must operate preclude detailed treatment.

**ASSESSING THE IMPACT OF THE NEWS MEDIA**

As a group with a specialist viewpoint, the nuclear industry tends to make two errors when interpreting the impact of the news media.

**Everybody's Looking at Me!**

The first error might be described as the error of disproportionate self-consciousness and could be analogized as the feelings of a gentleman who has just suffered a rent in his trousers on his way to work. He feels acutely conscious of the fact and will feel that the damage is humiliatingly apparent to those about him. But in fact it is unlikely anyone will notice since most of the people in the vicinity are going to have many other things on their minds than the possible existence of a man with a hole in his trousers. A newspaper story or a TV programme about nuclear energy leaps to our attention because we are in the business, and we will scrutinize that story or broadcast painstakingly and probably spend significant time and emotional energy second guessing its impact on the general public. But for people not in the business the story or broadcast is one of many and it takes its place among a list of dramatically presented impressionistic accounts of various events and issues of the day.

**There's No´t Sermon In This Stone!**

The second error I believe we make is that of too literal interpretation of what, as has been argued
earlier, is an impressionistic form of communication. Noting that a journalist must attract and hold the attention of an audience it should not be surprising that the journalist will employ the linguistic and visual tools of the novelist, dramatist and cinematographer. Additionally the use of what one might define as metaphors (whether linguistic or visual) is important in helping the journalist meet the time and space constraints of the news media.

Applying representational standards of interpretation to an impressionistic piece of work is profitless. We must recognize that the nature of communication via the news media requires the elements of entertainment and drama. That a newspaper story does not conform to our own standards of communication is as significant an observation as noting that a letter to an aunt will not conform to the standards applied to a letter to the Atomic Energy Control Board.

The Perspective

It is very important to retain a sense of perspective when we consider the impact of the news media on society (or the impact of society on the news media). Our society rarely (if ever) reacts instantaneously to a single stimulus and this is just as well. To do so would be to make the mistake of responding to a single piece of data in isolation. Through the news media our society is presented with a large number of quite brightly coloured data points. Some individual events will stand out-- a reactor accident at Chernobyl, a collision of two Boeing 747 airliners at Teneriffe or a railway crash at Hinton-- but unless these events are part of a pattern of similar events the impact is short term.

The impact is short term because most people realize, consciously or subconsciously, that while airliners do crash the overall safety record of civilian air transportation is acceptable, that while railway trains do collide a train is not an unduly hazardous form of transportation. And I believe that time will show that in this country at least, while people realize that reactors can have accidents, they regard nuclear reactors as an acceptably safe form of energy conversion. Inherent to the general public is the kind of pragmatic common sense that places various issues in some kind of perspective and judges by results. In 1981 there was a provincial election in Ontario-- almost exactly two years after the Three Mile Island accident and following an extensive (and very well publicized) investigation into nuclear safety by a Select Committee of the Legislature. In the election campaign one particular party took a strong anti-nuclear energy stance. That party lost about ten seats. This did not signify a ringing endorsement of Ontario Hydro's nuclear energy programme by the electorate but it did indicate, that within the overall context of the concerns of the voters, nuclear energy was of low priority.

Actual performance is the criterion by which nuclear energy will be evaluated by the general public, and if that performance remains acceptable then nuclear energy will remain acceptable. It is said that stinking fish are their own best warning. So long as our own fish don't stink we have little to fear from even the most sensational newspaper coverage or the most misleading television broadcasts.

CAN WE DO MORE?

The nuclear industry should not attempt to evaluate the news media by our own communications standards nor should we in any way seek to impose those standards upon the news media. This is an objective neither attainable nor desirable. Our interaction with the news media should be limited to providing journalists with the most usable, timely and candid information we can and correcting gross errors or misrepresentations. It is the input to the media system with which we have to be concerned-- the output is the responsibility of the professionals in the field. In my own department at Ontario Hydro there exists an informal and unwritten agreement to the effect that we don't tell journalists how to write their stories, and they don't tell us how to get reactor licenses.

But the essential message about the news media is that so long as we do our jobs properly, so long as CANDU reactors maintain their record of safe, economic operation and so long as we address the question of fuel waste management with technical competence and despatch, nuclear energy from CANDU reactors will continue to be regarded, in Ontario at least, as an acceptable energy system.

THE NOT SO GENERAL PUBLIC

The "not so general public" comprises those groups, formally constituted or otherwise, with influence upon the development or selection of future energy policies. The nuclear engineering community is not over-represented in such groups. Additionally there exists a dearth of readily available technical literature on the engineering specifics of CANDU reactors. These facts suggest that there exists the serious danger that future energy policy development could be predicated upon misconceptions about the advantages nuclear energy offers and the demands that it makes.

A small but significant example of the sort of problem that can arise is the case of a quite eminent resource economist who firmly believed that one objection to increased reliance upon CANDU reactors in the energy budget was the fact that these units could only be satisfactorily
operated at full power—load following was not possible. This idea was based not on reading newspaper or magazine articles but on inferences drawn from Ontario Hydro publications.

The nuclear energy community in general, and the Canadian Nuclear Society in particular, should as a matter of some urgency consider means whereby the appropriate intellectual tools to evaluate nuclear energy may be given to those groups with any kind of influence on the formulation, evaluation or implementation of energy policy. These range from elected politicians, through their immediate advisers to the civil service, and through a whole range of groups and educational institutions.

What is being argued for here is an educational programme involving people close to the technical working level of the industry, designed to make nuclear energy intellectually accessible to the intelligent non-engineer. If we can give our energy policy planners at least enough grounding in the subject to know how to ask the right questions, then the long-term future of nuclear energy will not be in doubt.
SESSION G: FUSION II

**Chairman:** R.A. Bolton, Hydro Quebec IREQ

Modelling of tritium dispersion in the atmosphere.
R.R. Bell, Monserco Limited

Ontario Hydro Research Division tritium handling system.
W.T. Shmayda, Ontario Hydro

Safety issues relating to the design of fusion power facilities.
R.R. Stasko and K.Y. Wong, Canadian Fusion Fuels Technology project and S.B. Russell, Ontario Hydro

Bulk getters for tritium storage.
N.P. Kherani and W.T. Shmayda, Ontario Hydro

The CRNL tritium laboratory — a Canadian resource.
W.J. Holtslander and J.M. Miller, AECL CRNL
ABSTRACT

The current version of the Ontario Hydro Tritium Dispersion Code has been used in a multi-national code comparison. During the course of this work a number of changes to the code have been made to improve the simulation. These changes and the most recent comparisons are described in this paper.

INTRODUCTION

The reason for the interest in tritium modelling in the environment is the extreme differences in dose effect between the elemental and oxide form (about a factor of 10,000). Elemental tritium released to the environment can be oxidized on contact with the ground by microbe action. How rapid this occurs is of major importance to safety assessment.

The Ontario Hydro Tritium Dispersion Code (OHTDC) is a computer model used to predict atmospheric dispersion and environmental recycling following a release of tritium gas in either the elemental or oxide form. The code calculates tritium concentrations in air and the ground. Oxidation of tritium in the ground and subsequent resuspension is accounted for in the model.

This paper describes some recent developments in the OHTDC model. These changes have been made to improve the simulation of recent laboratory scale experimental results.

Some results, from full scale tests and unintentional releases are available in Ref. 1 and 2, but the measured oxidation rates vary enormously. A new full scale test is to be undertaken this month in Saclay, France and some code comparison work was done as an aid to planning this experiment. France, Italy, Sweden and Canada were involved with this comparison. Some preliminary results are given in this paper.

BRIEF CODE HISTORY

The original Ontario Hydro Tritium Dispersion Code (OHTDC) was written about four years ago to compute the dose effect of a release of elemental or oxidized tritium. The first version was put together relatively quickly to determine if the overall concept of integrating atmospheric dispersion and diffusion into and out of the ground would work. Since that time, numerous modifications have been made to improve accuracy and running time. A more detailed description of the code is given in Ref. 3, but a brief description of the parts involved in the modifications is given here. The basic concept of the code is indicated in Figure 1. A release from a source forms a plume over a specified grid, with each grid area characterised by specific properties such as roughness, soil diffusion, population density etc. The released tritium can be absorbed by the ground, and if it is in elemental form, it can be oxidised and then be redispersed as indicated in Figure 2.

FIGURE 1

BASIC MODEL

Major revisions to the code have been made in two areas, first the atmospheric compartment and secondly the soil compartment. The type of modifications and the reasons for them are outlined below.
In the original version, the airborne concentration of tritium was calculated at the center of gravity of each grid element using the standard Gaussian plume equations. Tritium deposition on to the soil surface of the grid element was calculated using this CG value. Fairly early in the bench marking it became apparent that because of the cross-plume Gaussian concentration profile this assumption could lead to gross errors depending on the mesh scheme chosen to define the elemental areas. The problem is indicated in Figure 3. By computing the average concentration over an elemental area, and using this for the soil deposition, a significant improvement was achieved. The method is still however dependent of mesh selection as indicated in Figure 4. This sensitivity is mainly apparent in the initial 'foot-print' mode. When long term 360 degree dispersion is considered the results are not as sensitive to mesh selection.

The second problem that became apparent with the original version was the computation of the plume depletion. Due to deposition on the ground, the concentration at the plume centerline is reduced below the theoretical value with no deposition. The reduction is a function of the deposition velocity and the weather class.

Because of the irregular mesh scheme allowed, the code must be able to integrate along the plume, using the correct deposition velocity, corresponding to the elements it passes over, as indicated in Figure 5. Originally, this search was done by trial and error each time the deposition velocity was required because it was easier to program. Since all elements can act as sources of re-emitted tritium oxide, this was a considerable computing effort.
To improve computation speed the code was modified so that the trial and error search for the elements under the plume was done only once on the first step and the results stored for subsequent use. The storage requirement is not insignificant. Assuming 200 elemental areas and 10 integration points, the code carries an array of $200 \times 200 \times 10 / 2 = 200,000$ element numbers, just for the plume depletion calculation. In fact, it has been found for a large number of cases, neglecting the plume depletion computation does not significantly change the results. This would apply for distances less than 10 km and for weather class equal to or greater than D.

GROUND COMPARTMENT

In the original version of the code, not much effort was expended on the computation of diffusion of gas in the ground. It was felt that it was reasonable to assume instantaneous oxidation of the tritium as it came into contact with either vegetation or the bare soil. This assumption meant that only the tritium oxide diffusion had to be considered. The model used is illustrated in Figure 6. The problem was that the model did not accept standard numbers such as total depth of soil to be simulated and diffusion coefficients. Each term in the model had to be calculated by hand, assuming a certain representative depth for each block. Although some test data could be simulated, it was not possible to test the sensitivity of the model to the simulated soil depth etc. The model was then modified as indicated in Figure 7. This model retained the original assumption of instantaneous oxidation of the tritium gas at the surface, but otherwise it was a standard diffusion problem. By convergence tests it was found that the number of nodes in the finite difference solution could be reduced if the length steps varied in a geometric progression.
For all test comparisons, the vegetation part of the model was not used because of lack of experimental data. The model worked well with the Garland data (Ref. 4), which involved exposing the ground surface to a fixed concentration of tritium oxide as indicated in Figure 8. The model did not work so well for Ogram's data (Ref. 5) for elemental tritium above ground as discussed later.

COMPARISON OF OHTDC SOIL MODEL WITH GARLAND'S DATA AND MODEL

At the meeting, to compare the initial computer results, more small scale French test data was given out that confirmed the test data from Ogram. This meant there was in fact a significant difference in concentration profiles between exposing the ground to tritium or tritium oxide. To account for this difference, the soil model was further modified as shown in Figure 10. So far, in this model the vegetation simulation has been dropped on the basis that, up to now it has never been used because of lack of suitable input data. It can readily be added again if required. The third revision now allows the diffusion of both tritium and tritium oxide to be simulated, and oxidation takes place within the ground at a fixed rate. Using this model, the original HTU data of Garland is still matched, and now it is also possible with the same model to match Ogram's data as shown in Figure 11 and Caput's data from Ref. 6.

However, it was this model that was used in the first round of intercode comparisons. One set of results are shown in Figure 9. The benchmark case involved the release of 3000 Ci of tritium gas over a 30 minute period, from a 40 m stack. Tritium and tritium oxide concentrations as a function of time were compared for all models.

The Swedish model, at the time, did not have inground oxidation. The French and Italian results were similar and the Canadian results showed a much faster decrease in oxide concentration after the passage of the tritium plume.
Using this revised model, the intercode comparison was then redone, and some of the results are given in Figures 12 and 13. The data used by the Canadian version is indicated in Table 1. The main unknown variable is the inground oxidation rate. The effect that this has on the results is indicated in Figure 13.

TABLE 1

REFERENCE CASE INPUT DATA

<table>
<thead>
<tr>
<th>RELEASE QUANTITY (Ci)</th>
<th>3000</th>
</tr>
</thead>
<tbody>
<tr>
<td>RELEASE DURATION (min)</td>
<td>30</td>
</tr>
<tr>
<td>RELEASE HEIGHT (m)</td>
<td>40</td>
</tr>
<tr>
<td>WEATHER CLASS</td>
<td>D</td>
</tr>
<tr>
<td>WIND SPEED (m/sec)</td>
<td>3</td>
</tr>
<tr>
<td>HT DEPOSITION VELOCITY (cm/sec)</td>
<td>0.02</td>
</tr>
<tr>
<td>HTO DEPOSITION VELOCITY (cm/sec)</td>
<td>1.8</td>
</tr>
<tr>
<td>HT DIFFUSION COEFF. (m^2/sec)</td>
<td>5E-6</td>
</tr>
<tr>
<td>HTO DIFFUSION COEFF. (m^2/sec)</td>
<td>2E-9</td>
</tr>
<tr>
<td>OXIDATION RATE IN SOIL (1/sec)</td>
<td>0.01</td>
</tr>
</tbody>
</table>
CONCLUSION

The full scale test that was used as a basis for the code comparison is scheduled to be completed this month. It is somewhat doubtful if the data obtained could be used to show which model is more accurate, but the data should indicate the overall validity of all models (or none of them). The main difference between OHTDC and the other codes is the time delay after the passage of the tritium gas before the concentration of tritium oxide reaches a maximum. This effect can be readily simulated in the revised soil model by preventing oxidation above a certain depth. The correct simulation could be verified by a simple laboratory experiment similar to the ones done previously, except that the concentration of both tritium and tritium oxide should be measured continuously during the test.

REFERENCES


ABSTRACT

A 1500 curie tritium laboratoy is being constructed at Ontario Hydro Research Division. A special feature of this laboratory is the tritium storage and delivery system (TSDS). This system can dispense up to 125 curies of pure tritium gas per hatch to experimenter's facilities within the laboratory. The system consists of two uranium storage beds, a circulation pump, an assay volume, a vacuum system and an analysis station. All components which routinely see high levels of activity are metallic. This paper describes the TSDS, outlines typical operating scenarios and discusses anticipated abnormal events and the recovery procedures.

1.0 INTRODUCTION

A demand for increased hands-on experience with tritium gas in a controlled manner is currently prevalent in Canada. This demand is arising on two separate although related fronts. The Tritium Removal Facility at Darlington NGS is in the advanced stages of construction. It is expected to handle a megacurie of pure tritium in the first year of operation alone. During its lifetime unforeseen problems will arise and will require solutions which have been experimentally verified. On the second front, the fusion group based in Ontario, Canadian Fusion Fuels Technology Projects, funds a broad range of fusion oriented research. Performance verification with tritium is desirable and quite often essential.

To meet these needs, a 1500 curie tritium laboratory has been designed and is under construction at Ontario Hydro Research Division. Laboratory commissioning is expected to begin later this summer. This facility has three objectives:

- to develop gas handling expertise in support of the Candu reactor, in particular the Tritium Removal Facility (TRF),
- to provide a service facility in support of the TRF,
- to carry out longer term R & D research using hydrogen isotopes with applications aimed at both fission and fusion.

2.0 TSDS FUNCTION AND DESIGN REQUIREMENTS

The TSDS can receive tritium gas from an external supplier either on uranium shipping containers or in gas bottles. The tritium will be stored as a uranium tritide in a double walled container. Two such beds in parallel are available in the loop. The design limit of each bed is 5000 curies. With the exception of tritium purification operations, the total loop inventory will be stored on only one of the two beds. The total on-site tritium inventory will be approximately 1500 curies and will be determined by pressure-volume-temperature measurements.

Helium-3 decay products and the ingress of glovebox atmosphere into the loop can degrade the purity of the stored tritium. The loop will incorporate a means of circulating the gas through the empty bed in the loop to separate the tritium from the inert gases. The tritium purity will be measured by radio frequency mass spectrometry capable of 10 ppm resolution in the low mass ranges.

Measured quantities of tritium ranging from 5 millicuries to 125 curies per batch can be delivered to experimenter's vessels. Prior to a filling operation, the facility will evacuate the experimenters' vessel to 10^-9 torr (10^-9 Pa) or lower and confirm its leak tightness. Vessels with leaks greater than 10^-8 torr-litres/s (10^-9 Nm/s) helium will be rejected.

Tritium releases to the environment during each of the operations will be maintained as low as reasonably achievable. Particular emphasis has been placed on recycling as much of the tritium as possible and on minimizing the conversion of tritium into its oxide form. Redundant paths are required between the critical components to increase system reliability and flexibility. A safety requirement for the loop is its ability to " rundown" into a " safe condition.

3.0 PROCESS DESCRIPTION

The tritium flow paths through the TSDS are shown in Figure 1. The discussion of the TSDS flow processes is based on this figure. The basic elements of the process are a uranium storage bed, an assay volume and a circulation pump.
The system receives, converts and stores tritium in the loop from an external source. For the unloading operation, the tritium supply container is attached to port A, unloaded into the holding volume and assayed by the pressure-volume-temperature method. The isotopic purity is verified by residual gas and analysis. Subsequently the tritium is loaded on one of the two uranium storage beds (USB). In these beds, the tritium is converted and stored as uranium tritide.

Gas is regenerated from the storage beds by controlled external heating. The tritide dissociates to form tritium gas and raw uranium metal. Desorbed tritium is pumped into the assay volume during the bed heating phase until the required pressure for filling the experimenter's vessel at port B is reached. After the gas is pressure equilibrated with the experimenter's vessel, the vessel is isolated from the loop by a valve located at port B. Gas remaining in the assay volume and in the line interconnecting the loop to the experimenter's vessel is returned to the storage bed before the vessel can be removed from the loop. (1,2)

If tritium is held in a uranium bed for an extended period of time, the bed must be purged of the helium-3 decay product before any tritium delivery operation. The removal of helium-3 entails circulating the T3/He-3 mixture over a second uranium bed. The T3 gas forms a new tritide. The residual helium-3 does not combine chemically with the uranium and can be evacuated from the system. Other impurities such as oxygen, nitrogen and carbon if present are eliminated automatically by self-purification through the gettering action of the uranium. These impurities form stable chemical compounds with uranium and have negligible dissociation pressures at the maximum USB operating temperatures. (2)

4.0 LOOP DESCRIPTION

The TSDS process loop is shown in Figure 2. The gas analysis system is illustrated in Figure 3. All the equipment in the process and analysis loops that are exposed to tritium gas are located in an inert atmosphere glovebox inside the tritium laboratory. This permits system operation, inspection and maintenance with negligible personnel exposure. TSDS critical components are mechanically supported to ensure tubing stresses are within allowable limits. Most components shall be evacuated, purged, cleaned and handled with gloves provided in the gloveports.

The process system is a stainless steel loop which consists of a dry circulation pump, two uranium storage beds, an assay vessel and bellows sealed pneumatically actuated valves. One centimetre internal diameter stainless steel bellows sealed pneumatically actuated valves. Where feasible, welded connections are used to minimize leakage. VCR fittings (3) are used if component removal is required for maintenance or replacement. In these fittings, leak-free joints are produced by compressing copper gaskets between two stainless steel semicircular edges.

The arrows at the valve locations denote the orientation of the valve rather than any flow direction. The valve seat is located at the arrow tail. The particular orientations have been selected to provide defined volumes in some portions of the loop, as in the case of the assay volume, or to minimize the optional and magnitude of leakage as in the case of the valve leading to the experimenter's vessel. In the latter example, only leaks across the seat can contaminate the loop with impurities. Leaks across the bellows gasket or failure of the bellows will not impact on the loop integrity.

Temperature measurements are made with chromel-alumel stainless steel sheathed thermocouples. All critical components carry redundant thermocouples, two for feedback control and two for monitoring. Pressure measurements are made with absolute capacitance manometers. The accuracy of the head
ranges from 0.5% to 2% depending on the requirement within the process loop.

The secondary containers of the uranium storage beds are outfitted with a 1 cm diameter argon backfill line and a capillary sampling line. The backfill line also serves as a vacuum line. Under normal operating conditions, the secondary volume is under vacuum. The tritium ingress due to permeation or leakage across a weld can be monitored in this region by a capillary. To reduce the cool down time of the USB, the secondary volume can be backfilled with argon to improve the heat transport coefficient from the primary container to the environment. Under abnormal conditions, this volume will be backfilled to 1 atmosphere argon automatically.

**FIGURE 3: GAS ANALYSIS LOOP**

The gas analysis loop provides the required vacuum for the process loop, the USB secondary volume and the gas analysis system. The vacuum system consists of a turbomolecular pump modified for tritium service backed by a helium tight mechanical pump. The line between the two pumps contains a tritium monitor, an isolation valve and a getter bed. The mechanical pump exhausts via an oil demister directly to the tritium laboratory stack. If the tritium concentration in the line between the two pumps exceeds a prescribed level, the isolation valve is closed, the foreline is vented with argon and the process loop is run down.

Trace quantities of tritium are removed from the turbo exhaust before they reach the mechanical pump in the foreline getter bed. Despite the precautions to minimize activity buildup in the mechanical oil, tritium levels will gradually increase in the oil and will present perhaps the most significant personnel hazard. To mitigate the potential of this hazard, provisions to change the mechanical pump oil remotely will be built into this pump. An oil reservoir and a contaminated oil container are hard plumbed into the mechanical pump lubrication circuit. To change the oil, a moderate vacuum is drawn on the contaminated oil container with the mechanical pump. Spent mechanical pump oil is vacuum drawn into the contaminated oil container. Subsequently a fresh charge from the oil reservoir is metered into the mechanical pump by gravity feed. Mechanical pump oil changes are to be carried out at minimum once every 6 months. Once the contaminated oil reservoir has been filled, it will be immobilized and packaged in accordance with standard Radiation Material Laboratory practices and shipped to a permanent waste storage site.

Turbo exhaust can be re-routed back to the process loop. The pumping speed of the spiral pump approaches zero when the upstream pressure drops below 0.6 Pa. (4,5) This represents a concentration of 0.2 millicuries/cc. In the event the loop must be opened or contents of a storage bed must be removed, reduction of this concentration in the line is desirable. A 500 fold reduction is attainable by valving the turbo into the process loop to replace the spiral pump.

The gas analysis system is fed by capillary lines which interconnect the main line of the process loop (at the assay vessel/experimenter's vessel location) and the USB secondary volumes to the molecular leak. The capillary lines are used to reduce the pressure in the sampled region to a manageable level, on the order of 1 kPa. The molecular leak provides a quantitative measurement of the tritium throughput into the analysis sector. The use of capillary lines without a pinhole leak results in unacceptably high tritium losses to the turbo. The use of a pinhole without a capillary feed line restricts the range of the pressure which can be used in the sample volume. The total gas exhausted from the pinhole and its composition are measured in the gas analysis system with a calibrated ionization gauge and a residual gas analyzer. (6)

5.0 INSTRUMENTATION, CONTROL, AND MONITORING

The TSDS is designed to provide metered quantities to experimenter's vessels. Routine loop operations will be carried out manually or automatically. Non-routine and off normal operations will be carried out manually. In all cases the operational status of the loop must be apparent at all times. This requires accurate measurement of pressure, temperature, purity and activity. The USBs require redundant instrumentation.

Temperature monitors are available for the shipping uranium beds, the two USBs, the assay line, the circulation pump, the two vacuum pumps, and the glovebox atmosphere. Pressure monitors are available at the experimenter's vessel location, in the assay line, the input to each USB, at the discharge end of the circulation pump, in the analysis section, on the intake of the foreline pump and in the glovebox. Tritium activity monitors are available at the intake of the foreline pump, and in the glovebox. Both monitors will have high activity alarms to indicate malfunctions in either the loop operation or the glovebox cleanup system and to take automatic remedial actions described in the following section.
Both local and remote monitoring are available. Local monitors are located in the vicinity of the TSDS and are intended for continuous monitoring and display of the status of various key components. Remote monitoring and control uses a computer driven data acquisition system. Direct display of equipment status is not given although this information can be called up on the CRT. The purpose of this monitoring is to record on a continuous basis the status of various TSDS components for future reference, in alarm abnormal conditions and to control processes. Remote loop control is automatic or manual but via the computer. In the latter case the operation is menu driven and displayed on a panel which bears a pictorial representation of the TSDS. Three automatic operations will be installed in the computer initially charging the experimental vessel, helium-3 removal and automatic shut down for some off-normal conditions.

6.0 CASUALTY EVENTS AND RECOVERY PROCEDURES

Table 1 gives a failure mode and analysis of the effect for the TSDS. At least one failure is postulated for each of the major components. Additionally, line breaks throughout the system are considered. In each case, the local effects, detection methods and corrective actions are discussed. Only those failures which may result in the release of tritium to the environment are considered. A variety of nuclear malfunctions can occur without resulting in any tritium release. In the placement of loop diagnostics, an effort for redundancy at critical points has been made to minimize the potential that an errant monitor will give operators incorrect information.

6.1 Unloading Shipping Containers

There are two major types of tritium shipping containers: gas bottles and uranium beds. In either case, the shipping container is attached to the loop in the glovebox. The leak tightness of the joint is checked before any transfer of gas is effected. The equipment is connected to the mains line. In the case of gas bottles, the majority of gas bottle contents will be transferred to the loop within a few minutes. In the case of uranium beds, transfer of the uranium shipping contents is expected to take on the order of 30 minutes. Two possible abnormal scenarios are envisioned. The connecting joint or a weld joint may "loosen" during the transfer. The spiral pump may fail during the transfer. While the latter does not result in a release of gas in the line is in a vulnerable condition. The potential for release in higher.

6.1.1 Rupture of the Connecting Line of a Weld. This can occur during the unloading of either a gas bottle or a uranium bed shipping container. A sudden total release into the glovebox is unlikely. A nominal nearly constant release rate during the unloading sequence is more likely. During this time, tritium will be released into the glovebox. The box activity monitor will alarm. The glovebox cleanup system will go into its re-circulation mode to getter the escaped tritium. In the uranium shipping container case, the turbo can be valved into the circuit to reduce the line pressure and consequently reduce the leakage rate 500 fold. If the leak rate fails to improve the shipping container will be cooled to room temperature, decoupled from the loop, packaged into second air tight metal container in an argon atmosphere and sent to waste storage for permanent disposal. Surface samples of the box and shipping bed will be required to establish if uranium particulate has escaped from the bed. If the loss rate decreases as a result of the lower loop pressure, the unloading cycle will continue until complete. The shipping container will be scrapped.

6.1.2 Loss of Pumping Capacity During the Unloading Operation. The probability of this event occurring is highest during the unloading of the uranium shipping container since the spiral pump must be operated for long periods of time. The most probable failure scenario is a seizure of the pump spirals or the motor. A sudden release into the box is unlikely. Permeant losses through the hot uranium bed will modestly increase the tritium activity level in the box. The process loop pressure will increase. The pump head pressure will drop. Remedial actions are: isolate the spiral pump and valve in the turbo, exhaust the turbo directly to the standby TSDS storage bed, continue the unloading to completion, filter the loop with a portable spiral pump from the loop and replace the defective component.

6.2 Line or Valve Failure

Operations with loop pressures above the glovebox pressure are not planned but could arise as a consequence of overpressurization due to a malfunctioning spiral pump or storage bed heater. During such an abnormal operation, the loop pressure will increase steadily but at a finite rate. Pressure transducers will record the loop overpressurization and cut all electrical power to the process loop. All valves will fall closed. Both USB secondary volumes will be backfill to 1 atm; here argon pressure.

Such a system failure is unlikely but if it occurred it would result in a large tritium release to the box. The line contents between the two functional valves where the rupture occurs will release suddenly to the box until the pressure in the loop equilibrates with the box. The concomitant pressure excursion in the box will be small. The glovebox cleanup will activate as described earlier. Tritium in the remainder of the loop is returned to the standby bed. The defective portion is replaced using the procedure described below.

Line or valve lineleakage when the loop pressures are below the glovebox pressure will be one of the most likely abnormal operating scenarios. Two failure modes are foreseen: one with a slow argon inleakage and one with a rapid inleakage. In the former case, the pressure will increase gradually but at a finite rate. A small increase in the box activity may occur. Corrective actions are: isolate the defective section, return any tritium gas to the loop operation. A rapid inleakage over the standby bed to remove the residual tritium, isolate and replace the defective component.

In the latter case, the inleakage will result in a sudden increase in the loop pressure. This scenario poses the most difficult and time consuming condition for recovery to normal loop operation. Prior to any operation with tritium, unused section of the process loop will be evacuated and isolated from the remainder of the process loop as a precaution against this accident scenario. In response to a rupture, manual shutdown of all valves, USB heaters and circulation.
pump in the process loop is required. The gas analysis loop will remain functional and will be used to determine the residual tritium content in the various loop sections. Recovery from this accident entails reducing the line pressure between the circulation pump and the standby bed by volume expansion, circulation over the bed to remove the tritium and exhausting the inert gas. The objective of the operation is to reduce the line pressure between the circulating pump and the standby bed to approximately 1 kPa so that circulation over the standby bed can be implemented without pump overheating. The gas in the individual sections of the process loop will be subsequently expanded into the circulation/USB line, detritiated and exhausted. During the expansion operation the USB will be isolated from the process loop.

An inleakage which precludes gas circulation will require evacuation of the circulation/USB section of the loop through a spare USB with the gas analysis system in a once through mode. Glovebox argon will be drawn through the defective part until the tritium concentration is acceptably low. The defective section will be replaced. Circulation as described above will be subsequently implemented to return all residual tritium to the USB. The spare bed contents will be returned to the loop USB.

6.3 Uranium Storage Bed Rupture

The probability of a USB rupture is slight. The development of a leak from the primary vessel to the secondary container is more likely. In either case, tritium activity in the secondary container will increase. The capillary line (SI) will signal the off normal conditions. Bed contents will be transferred to the standby USB. If necessary the secondary container contents can be circulated over the standby USB. No loss to the box is expected.

6.4 Glovebox Overpressurization

Glovebox overpressurization will be most likely due to a failure in the argon gas supply system. The box pressure sensor will alarm. The process loop is shut down. Passive pressure relief via a bubbler arrangement set at 4" of water is activated. The gas supply flowrate is choked to a maximum throughput. The pressure relief system is sized accordingly. This off normal operation has no impact on the process loop. A slight increase in the stack emissions is expected during this event.

6.5 Glove Inleakage

Air inleakage will most likely result from a

rupture in one of the butyl gloves. One of the three detectors for oxygen, nitrogen or water vapour within the box will alarm and shut down the process loop. This condition will develop slowly and provide sufficient advance warning to permit rebagging the defective port without any noticeable increase in stack emissions.

7.0 SUMMARY

All the features implemented in this loop design are based on gas handling experience developed at Ontario Hydro Research. Components similar to those purchased for the tritium supply and delivery system have been tested under conditions anticipated in the TSDS. The process loop has been designed with redundancy to permit recovery from all the abnormal conditions conceived. Operating procedures to mitigate some of these conditions have been identified and will be implemented. Particular attention has been paid to minimize the quantity of tritium vulnerable to release to the glovebox during any given process operation. Protium will be used during the commissioning phase to test the system response to these abnormal conditions.

8.0 REFERENCES

(3) Cajon, VCR stainless steel vacuum couplings.
(4) Normetex, 27500 Pont-Audemer, France.
(5) Unpublished data on pump speed measurement made at Ontario Hydro Research.
<table>
<thead>
<tr>
<th>Component</th>
<th>Failure Mode</th>
<th>Cause</th>
<th>Effect</th>
<th>Detection Method</th>
<th>Corrective Action</th>
</tr>
</thead>
</table>
| Shipping Container         | leakage/rupture       | valve, line breakage   | $^{3}$ release into glovebox  | $^{3}$ monitor alarm                   | - cool shipping container to room temperature  
|                            |                       |                        |                               |                                        | - clean the glovebox atmosphere using the box’s cleanup system                     |
| $^{3}$ line inside glovebox| leakage/rupture       | faulty weld, valve breakage | $^{3}$ release into glovebox or argon ingress into line | $^{3}$ monitor alarm or vacuum gauge/ pressure gauge alarm | - return remaining $^{3}$ to cold bed  
|                            |                       |                        |                               |                                        | - cool active bed  
|                            |                       |                        |                               |                                        | - clean glovebox atmosphere with box’s cleanup system                             |
| Transfer Pump              | failure to operate    | electrical/mechanical malfunction | loss of head pressure loss of flow | discharge pressure sensor            | - return $^{3}$ to U-bed  
|                            |                       |                        |                               |                                        | - flush pump with argon  
|                            |                       |                        |                               |                                        | - replace with spare                                                               |
| U-bed                      | leakage/rupture       | overpressurized box, faulty weld | $^{3}$ release into secondary container | $^{3}$ monitor alarm in foreline      | - maintain bed under vacuum  
|                            |                       |                        |                               |                                        | - transfer $^{3}$ to cold bed  
|                            |                       |                        |                               |                                        | - backfill faulty bed with argon  
|                            |                       |                        |                               |                                        | - replace with spare                                                               |
| Holding Volume             | leakage/rupture       | faulty weld, valve or transducer fitting | loss of vacuum in loop | pressure gauge $^{3}$ monitor alarm in foreline | - transfer remaining $^{3}$ to U-bed  
|                            |                       |                        |                               |                                        | - backfill volume with argon  
|                            |                       |                        |                               |                                        | - replace                                                                 |
| Glovebox                   | argon outleakage or air intleakage | overpressurized supply gas line glove port failure | argon release into $^{3}$ laboratory in ingress of $^2$ $O_2$ and $H_2O$ vapour into box | box pressure sensor $^2$, $H_2O$ and $N_2$ detectors | - return any $^{3}$ to U-bed  
|                            |                       |                        |                               |                                        | - turn off the power                                                                 |
SAFETY ISSUES RELATING TO THE DESIGN OF
FUSION POWER FACILITIES

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ABSTRACT

In order to make fusion power a viable future source of energy, it will be necessary to ensure that the cost of power for fusion electric generation is competitive with advanced fission concepts. In addition, fusion power will have to live up to its original promise of being a more radiologically benign technology than fission, and be able to demonstrate excellent operational safety performance. These two requirements are interrelated, since the selection of an appropriate safety philosophy early in the design phase could greatly reduce or eliminate the capital costs of elaborate safety related and protective systems. This paper will briefly overview a few of the key safety issues presently recognized as critical to the ultimate achievement of licensable, environmentally safe and socially acceptable fusion power facilities.

INTRODUCTION

First-generation fusion reactors will most likely be based on the deuterium - tritium fusion reaction:

\[ D + T \rightarrow 4\text{He} + n + 17.6 \text{ MeV} \]

The use of tritium as a fuel requires handling of substantial quantities of tritium. The highly energetic 14.1 MeV neutrons will produce activation products in structural materials although the nature and quantities of activation products can be controlled to some extent by the development and use of low activation materials. On the other hand, there are no fission products to deal with.

In order to identify key fusion technology issues which will have potentially adverse effects on the environment, it is necessary to perform some preliminary environmental assessments based on some reference/generic facility designs. In order to scope the magnitude of the potential problems, several comprehensive assessments of this nature have been performed (1)(2)(3)(4). The consensus appears to be that fusion power facilities have the potential to be more radiologically and environmentally benign than fission plants of comparable capacity. Even in the case of low probability events which result in massive releases of tritium and other volatile radionuclides the calculated dose impacts on the exposed population are judged to be approximately two orders of magnitude less severe. While this makes the task of engineering safety into the facility design somewhat easier, there are still many areas of concern which must be addressed very early in the design stage. The engineering goal must be to ensure excellent performance with respect to public risk, emissions of radionuclides to the environment, and exposure of facility staff to radiation hazards without impacts on the cost of power sufficient to impair any competitive advantage fusion might have over other energy forms.

In this paper we would like to take a slightly different approach, and try to first define some key radiological criteria which can be judged in isolation of potential source terms to establish an acceptable basis for licensable, environmentally safe and socially acceptable fusion power facilities. Once some reasonable quantitative targets and limits are established which are judged to meet overall societal/regulatory expectations of acceptable hazards and risk, the ease or difficulty of achieving those goals with present technology and facility design concepts can be considered. The intent will be to identify those safety related areas which are still critical issues and which must be addressed in future R&D programs and design studies.

THE ISSUE OF INHERENT SAFETY

Experience worldwide with fission power reactors has demonstrated that the cost of addressing safety in design can be very high. So high in fact that many utilities presently considering the expansion of their generating capacity are avoiding nuclear plants. High capital costs, coupled with uncertainties as to investment risk, public liability issues and ever-increasing regulatory quality assurance (Q/A) requirements do not make this the option of choice in the US or in Canada at this time.
One major impediment in dealing with nuclear safety in fission plants is the need for active, responsive safety systems which must respond to process upsets or system failures. These safety systems are often complex and expensive, yet they must be highly reliable. Requirements for duplication of safety systems, triplexing of control channels, hardening against common mode effects such as seismic events, high quality engineering and Q/A costs, and most importantly, demonstration with respect to system adequacy make these systems expensive to build and maintain. Due to the high power densities, fuel afterheat and geometry-dependent cooling requirements of the present generation of fission plants, there are no alternatives to the concept of system safety which relies on active safety systems. Even at this very early stage in the development of future fusion power concepts, it is important to select that safety design philosophy which will best exploit the advantages of fusion technology, and which will compete favorably with other future energy options. The two areas of most significance, cost of power and safety, are very interdependent as is evident in the present design of operating power reactors.

Due to an undercurrent of public dissatisfaction with the present generation of fission plants, a large effort is underway worldwide to develop inherently safe fission reactors, and a number of innovative designs have already emerged such as the Process Inherent Ultimately Safe (PIUS) reactor, and the Modular High Temperature Gas Reactor (MHTGR). In order to compete with these advanced concepts, fusion reactors will also have to embody 'walk-away safe' concepts which rely solely on the inherent conductive and radiative heat transfer properties of the materials comprising the blanket and first wall.

Piet (5) defines an inherently safe facility as one which embodies passive safety features such that the public is protected from any acute fatalities under all credible accidental circumstances. Inherent safety also requires that this level of safety is achieved by passive design features, rather than active engineered safety systems, to enhance demonstrability and reliability.

Piet and Logan (6) have both provided insight and rationales for the application of inherent safety concepts to the design of fusion power facilities. Both make the case that the engineering challenge of designing inherently safe facilities is a vital part of the overall development of fusion power. Logan specifically links the ultimate economic success of fusion to our collective ability to effectively exploit the safety advantages implicit in fusion technology; especially as they prove amenable to inherent safety concepts. Some of the obvious advantages are:

* No fuel afterheat to contend with
* No criticality issues
* No buildup of large inventories of fission products

Also, in the case where accidents result in the release of radionuclides, tritium will be the critical radionuclide, followed by activation products such as Fe-55, Co-60 and Ni-63. Worst case accidents are expected to result in off-site doses (individual and collective) which are 100 to 1000 times less severe than for fission plants of similar power ratings. This is because:

1. The total curie inventory of radionuclides that can be mobilized, released and transported is smaller
2. The Biological Hazard Potential (BHP) of the tritium and the activation products are less than for actinides and fission products
3. Once released to the environment, tritium disperses much more rapidly through the ecosystem than most fission products and actinides, thus reducing concentrations (e.g., in soil).
4. Tritium does not concentrate anywhere in the food chain, or in the human body, and has a short biological half-life in any case.

Note that while other volatile radionuclides may be released under various accident conditions, tritium is generally considered to be the critical radionuclide in any assessment of acute hazards. The need to limit the mobilization, release and transport of tritium will very likely dominate the process of designing an inherently safe fusion facility. However, the engineering challenges are not insurmountable, and any disadvantages arising from lower thermal efficiencies and/or higher mass/power ratios will probably be more than made up by avoidance of:

* Complex, active engineered safety systems and safety support systems
* High balance-of-plant (BOP) costs associated with nuclear grade standards and specifications
* Large-scale nuclear grade containments (when high-quality confinement will do)
* Costly, time consuming regulatory/licensing requirements

If fusion technology is in a better position to benefit from inherently safe design concepts, which appears to be the case, the resulting advantages to be gained in the marketplace for energy and relating to public concerns about safety may be pivotal in the ultimate commercialization of this energy form.

**ACUTE RELEASE LIMITS**

In accordance with prevailing national regulatory and licensing guidelines, the construction of nuclear facilities must be seen as limiting off-site radiation doses to less than acceptable limits. The consequences of various worst case accidents must not exceed specified dose criteria to members of the public. Curie limits for the escape of various radionuclides are subsequently derived from these dose limits.

These values vary between countries and sites so we can only deal with this issue generically, or by examination of a specific example. However, in light of the discussion regarding the need for inherent safety, we can propose that for any site even for the worst-case credible accident (and subsequent releases) there should be no possibility of acute off-site fatalities. Only in this way can the design philosophy be verified as genuinely inherently safe. However, this scenario is still
only acceptable if all protective barriers (passive or otherwise) are assumed to fail, and only if the probability of occurrence is extremely small.

Recently, the Atomic Energy Control Board of Canada issued a set of accident release limits for trial use which balance the acceptable consequences of an accident with the probability of occurrence (7). Table I indicates the various decision levels, and the appropriate dose limits. It should be noted that the Darlington Tritium Removal Facility, which will have a steady-state releasable tritium inventory of approx. 100 grams, has been licensed using these guidelines.

**TABLE I**

**Radiation Dose Limits to a Member of the Public for an Event of a Given Frequency**

(From AECB Consultative document C-6 "Requirements for the Safety Analysis of CANDU Nuclear Power Plants, June 1980")

<table>
<thead>
<tr>
<th>Event Class</th>
<th>Event Qualitative Event Criteria</th>
<th>Frequency Criteria</th>
<th>Expected Event Frequency</th>
<th>Individual Dose Limit</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Greater than 50% chance of occurring in the lifetime of a single reactor, or more frequency than twice in the lifetime of a 4-unit station</td>
<td>$f &gt; 10^{-1}$</td>
<td>50 mrem</td>
<td></td>
</tr>
<tr>
<td>2</td>
<td>About once in the lifetime of an 8-unit station</td>
<td>$10^{-2} &lt; f &lt; 10^{-3}$</td>
<td>500 mrem</td>
<td></td>
</tr>
<tr>
<td>3</td>
<td>Low probability postulated failure</td>
<td>$10^{-3} &lt; f &lt; 10^{-4}$</td>
<td>3 rem</td>
<td></td>
</tr>
<tr>
<td>4</td>
<td>Very low probability postulated failure</td>
<td>$10^{-4} &lt; f &lt; 10^{-5}$</td>
<td>10 rem</td>
<td></td>
</tr>
<tr>
<td>5</td>
<td>Extremely low probability postulated failure</td>
<td>$f \leq 10^{-5}$</td>
<td>25 rem</td>
<td></td>
</tr>
</tbody>
</table>

We submit that fusion system safety criteria should be derived from a sliding scale similar in concept to Table I. Please note that we have not modified the worst case off-site dose from 25 rem. We feel that possible incorporation of the Acute Fatality Threshold (AFT) of 320 rem as part of any probability/consequence accident criteria would not be acceptable to nuclear regulatory agencies. The 25 rem value has been accepted worldwide as an upper bound for worst case accidents. The challenge with respect to designing for inherent safety is to make this dose limit a credible upper bound without the need for a great deal of elaborate and often questionable probabilistic risk assessments and (associated analytical support) applied to multiple barriers and active safety systems.

Several computer runs of the tritium dispersion code PATHWAY, developed at Ontario Hydro, have been completed with various postulated releases of HTO, for various classes of weather, and for two different release heights (0 and 60 meters). The results are presented in Figures 1 to 4. The assumptions are as follows: puff release of oxidized tritium, 1000 meter exclusion zone, open grassland, 5000 sq. meter building area. From Fig. 1 it can be seen that an acute stack release of 100 grams of tritium (as HTO) would result in maximum off-site doses of 600 mrem. Note from Fig. 4 that even with a ground level release of 1.0 kg of tritium as HTO, and under the worst weather classification, the off-site dose to the most exposed individual is approximately 42 rem, which is considerably less than the Acute Fatality Threshold of 320 rem, but is greater then the 25 rem lifetime dose limit recommended by the ICRP. Any facility design that could limit the tritium inventory to approximately half this value (500 grams), or could demonstrate the effectiveness of passive barriers which would limit the releasable inventory or release fraction to this level, could be considered inherently safe. Again, this assumes that the probability of occurrence of a catastrophic release is less than one event in every 10E5 or 10E6 years.
mrem/yr, with a mean value of 4.2 mrem/yr for ten
different codes run with the same data (see Table
II).

\[ TABLE \ II \]

<table>
<thead>
<tr>
<th>Code</th>
<th>Dose at 1000 M (mrem/yr)</th>
</tr>
</thead>
<tbody>
<tr>
<td>AIRDOSE-CPA</td>
<td>10.4</td>
</tr>
<tr>
<td>AIRDOSE-WXT</td>
<td>1.1</td>
</tr>
<tr>
<td>ANDOSE</td>
<td>1.1</td>
</tr>
<tr>
<td>GASPAR</td>
<td>5.5</td>
</tr>
<tr>
<td>OHTDC</td>
<td>6.2</td>
</tr>
<tr>
<td>OHTDC/PPL</td>
<td>9.3</td>
</tr>
<tr>
<td>PATHWAY</td>
<td>2.4</td>
</tr>
<tr>
<td>TREM</td>
<td>1.7</td>
</tr>
<tr>
<td>TRITHOD</td>
<td>2.7</td>
</tr>
<tr>
<td>UNIDOSE</td>
<td>1.3</td>
</tr>
</tbody>
</table>

**MEAN VALUE** 4.2

* Inhalation + skin absorption only.

Results from these computer code calculations may be
compared with experience at operating nuclear
facilities. CANDU nuclear reactors use heavy water
in both the coolant and moderator. Tritium oxide
releases and environmental levels are routinely
measured. Tritium oxide release data at the
Pickering Nuclear Generating Station near Toronto,
Canada and measured average boundary air tritium
oxide concentrations at approximately 1 km from the
release points for the period 1983-1985 are given in
Table III. Based on boundary air concentrations and
tritium levels measured in vegetation, milk and
drinking water, tritium doses received by the
critical group individuals are calculated (9) and
summarized in Table IV. Pathway parameters used in
the calculations are given in Table V. Dosemetric
parameters used are those in ICRP 30 (10). Doses
were calculated separately for the six-month old
infant and the adult. The physical release height
at Pickering NGS is 40m. However, the release
points are not isolated free-standing stacks and the
effective release height is less than 40m.
Based on Pickering experience, for a daily HTO release of 40 to 50 Ci, the dose to individuals is less than 1.0 millirem/year. Milk samples were taken at distances of 10 km and beyond. If there were dairy farms at the site boundary, milk ingestion doses could be 10 to 20 times higher and the total individual tritium dose could be 2 to 3 millirem per year. These data indicate that the computer code simulations for HTO release have not underestimated doses, and that there is reasonable agreement.

A design target of 50 Ci/day for chronic emissions of HTO will provide reasonable assurance that the design target of 5 mrem/year for individual members of the public will not be exceeded.

**Occupational Safety**

In any fusion power facility as presently envisioned, the bulk of maintenance work in the reactor hall will require remote handling systems, due to the high activation levels of first wall and blanket components. The biological shielding necessary for routine shutdown access is generally viewed as prohibitive. However, some shielding is necessary to limit neutron damage load on magnet materials/systems, and more is needed to avoid unacceptable activation of structural material in the reactor hall. Accordingly, some degree of personnel access should be possible, and is likely desirable for the purpose of 1) optimizing for small, relatively intricate tasks which would not be cost effective to perform remotely (e.g., connect/disconnect of coolant lines or electrical systems) or 2) ensuring a large degree of operational flexibility in response to unanticipated tasks for which no remote handling capability exists.

It should be underscored that the greater proportion of radiation dose accumulated by fusion facility staff is unlikely to result from exposures in high field areas such as the reactor hall. Experience indicates that most person-rem are accumulated through integrated exposure to lower radiation field areas such as heat exchanger and circulating equipment rooms, auxiliary equipment rooms and waste handling/cleanup system rooms. In these locations activated corrosion products which are transported outside of the primary reactor shield exposure result in radiation fields which, while not high enough to justify the cost of remote systems for maintenance, are nonetheless significant enough to require radiological work planning and dose control.

Recently, as part of the MINIMARS mirror fusion reactor design study [11], an attempt was made to establish realistic ALARA occupational exposure limits for staff at a reference MINIMARS facility. In order to establish preliminary design targets for various work locations and hazard levels, it is necessary to make an estimate of typical occupancies for the various areas. These estimates are summarized in Table VI, and are based on qualified extrapolations from experience at existing nuclear facilities. Note the key assumptions as follows:

- 80% power station availability (20% facility downtime)
- 2000 working hours/year/individual worker
- Staff levels based on labour dictated manpower requirements
  (i.e. not on person-rem requirements)

### Table III

**PICKERING NGS TRITIUM RELEASE DATA**

<table>
<thead>
<tr>
<th>Year</th>
<th>Release in Air (Ci/day)</th>
<th>Release in Water (Ci/day)</th>
<th>Boundary Air Concentration (pico-Ci/m³)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1983</td>
<td>48</td>
<td>27</td>
<td>473</td>
</tr>
<tr>
<td>1984</td>
<td>36</td>
<td>31</td>
<td>400</td>
</tr>
<tr>
<td>1985</td>
<td>40</td>
<td>52</td>
<td>274</td>
</tr>
</tbody>
</table>

### Table IV

**Doses to Critical Group Individuals from Pickering NGS Tritium Releases**

<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>Inhalation</td>
<td>0.26</td>
<td>0.27</td>
<td>0.20</td>
<td>0.20</td>
<td>0.17</td>
<td>0.18</td>
</tr>
<tr>
<td>Skin Absorption</td>
<td>0.08</td>
<td>0.013</td>
<td>0.10</td>
<td>0.016</td>
<td>0.11</td>
<td>0.018</td>
</tr>
<tr>
<td>Ingestion Fruit and Veggies</td>
<td>0.0018</td>
<td>0.0033</td>
<td>0.023</td>
<td>0.0035</td>
<td>0.024</td>
<td></td>
</tr>
<tr>
<td>Ingestion Milk</td>
<td>0.00028</td>
<td>0.00096</td>
<td>0.0016</td>
<td>0.0054</td>
<td>0.00054</td>
<td>0.0019</td>
</tr>
<tr>
<td>TOTAL</td>
<td>0.62</td>
<td>0.88</td>
<td>0.55</td>
<td>0.74</td>
<td>0.45</td>
<td>0.56</td>
</tr>
</tbody>
</table>

### Table V

**Critical Group Pathway Parameters**

<table>
<thead>
<tr>
<th>Pathway</th>
<th>Infant</th>
<th>Adult</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ingestion Rate (m³/a)</td>
<td>1400</td>
<td>8300</td>
</tr>
<tr>
<td>Ingestion of Fruits and Vegetation (kg/a)</td>
<td>100</td>
<td>300</td>
</tr>
<tr>
<td>Ingestion of Milk (kg/a)</td>
<td>365</td>
<td>170</td>
</tr>
<tr>
<td>Ingestion of Fish (kg/a)</td>
<td>0.8</td>
<td>8</td>
</tr>
<tr>
<td>Ingestion of Water (kg/a)</td>
<td>35</td>
<td>700</td>
</tr>
</tbody>
</table>
Table VI differentiates between the most exposed work group (e.g., mechanical maintainers, control technicians) and the total work force which includes technical support and admin. staff. Retrospective studies indicate that in general, when the most exposed work group averages 1.0 rem/year, the total exposed workforce averages 0.5 rem/yr of occupational dose (12). The ICRP (International Committee on Radiological Protection) guidelines state that the level of worker health risk associated with this level of exposure is equivalent to that for workers in conventional, safe industries.

For the MINIMARS design study, a design target of 1.0 rem/yr (average) for the most exposed work group was recommended. While design information is too preliminary to perform the required cost/benefit assessments, this target is reasonably consistent with NRC guidelines relating to application of ALARA principles in nuclear design. In addition, this target is in agreement with what is presently considered by the Health Physics community to be excellent performance at operating nuclear power facilities. As such, it provides a reasonable default value for operations/maintenance staff at any future fusion power facility under consideration.

In terms of design targets, the proportion of dose which should be assigned to penetrating radiation vs that for internal dose (tritium) is difficult to determine at this time. Based on an estimate of the relative costs associated with dose control, a ratio of 3/1 for external/internal dose will be assumed. Using this ratio, the proposed yearly dose targets and the occupancies in Table VI, a set of allowable area dose rates and airborne tritium concentrations can be derived. These are summarized in Table VII. Note that these are average values. Contact dose rates for specific pieces of equipment, or local MPCa levels can be higher if anticipated occupancies are proportionally lower. In addition, these values would not necessarily apply within 48 hours after shutdown, so long as the time integrated average is consistent with their intent. A more detailed explanation of the supporting logic for the values in Table VII can be found in Ref. 12.

Table VII shows annual average occupational dose estimates for the most exposed work group. The assumptions include 80% availability, 2000 working hours/year/individual, facility staff level based on labor dictated manpower (not person-rem req.).

<table>
<thead>
<tr>
<th>Radiological Work Area</th>
<th>Most Exposed Work Group (Average Hrs/Yr)</th>
<th>Total Exposed Work Force (Average Hrs/Yr)</th>
</tr>
</thead>
<tbody>
<tr>
<td>I</td>
<td>600</td>
<td>1000</td>
</tr>
<tr>
<td>II</td>
<td>1000</td>
<td>900</td>
</tr>
<tr>
<td>III</td>
<td>400</td>
<td>100</td>
</tr>
</tbody>
</table>

**NOTE:** These are averaged area values. Contact dose rates for special equipment, or local MPCa levels may be higher if anticipated exposure/occupancy is lower.

**NUCLEAR DESIGN STANDARDS**

In order to ensure the highest degree of component integrity and reliability, and as a part of the defence-in-depth philosophy in proof against single component failure, virtually all countries constructing nuclear power facilities call-up a special nuclear code or 'N-stamp' in the
specification of nuclear system components. Included in the often large list of systems and subsystems which require N-stamp in Canada and the US are the primary heat transport system (PHT), the emergency cooling systems, reactivity control and shutdown systems and the overall containment structure. Nuclear grade specification does not necessarily result in a component that is different from one that is commercial grade. However, the quality assurance requirements involving examination and testing procedures, tracing of source materials and supporting documentation can increase costs by a factor of three. Nuclear Design Safety Criteria for component specifications such as ANSI/ANS-51.1 in the US and the Canadian CAN3-W285.0 were developed to enhance fission power plant safety, and to better protect the public, nuclear workers, and to some extent owner investment. However, upon review of these standards it becomes evident that they were initiated to address the specific safety concerns associated with fission power reactors (LWRs in the US, and PWRs in Canada). In addition, any evolution of these standards has been in response to safety issues arising from fission reactor experience.

The current N class design standards do not properly reflect those safety design issues unique to fusion, and they do not allow for exploitation of those specific advantages embodied in fusion technology which would be amenable to inherent safety. For these reasons, it is recommended that in all future design studies the existing standards are not incorporated in the design and costing codes. This would avoid legitimizing inappropriate standards, when what is required is a new set of nuclear design standards for systems and components unique to fusion applications. It is important that some new guidelines in this area be generated as soon as possible, and that the decision criteria be based on radiological consequence of failure analyses (COFA), and not on edicts originally based on fission plant, and do not allow for proper exploitation of the inherent safety advantages of fusion technology. Application of these codes in design and costing studies should be discontinued until nuclear design codes specific to fusion are developed.

CONCLUSIONS

Inherent safety in design is a worthwhile engineering challenge for fusion power development. Application of this concept in ongoing conceptual studies may help to ensure that fusion is a viable future energy option.

In order to ensure that off-site radiation doses resulting from worst-case design basis accidents do not exceed 25 rem, the releasable tritium inventory should not exceed 500 grams. This should not prove to be an insurmountable design requirement.

Operating fusion facility chronic emission targets of approximately 50 Ci/day of tritium (to atmosphere) are very likely achievable, and will result in off-site doses of less than 5 rem/year from gaseous emissions.

An appropriate mix of remote and hands-on maintenance is likely the most cost-effective approach to fusion facility maintenance, since it provides for operational flexibility in response to those maintenance conditions which were not foreseen. Occupational doses equal to or better than what is presently regarded as good safety performance at operating nuclear facilities are likely achievable at fusion facilities.

Existing nuclear design codes were developed for fission plants, and do not allow for proper exploitation of the inherent safety advantages of fusion technology. Application of these codes in design and costing studies should be discontinued until nuclear design codes specific to fusion are developed.

REFERENCES


(2) HANCOX, R., REDPATH, W., Fusion Reactors - Safety and Environmental Impact, Culham Laboratory Preprint Report CLM-P750, 1985

(3) WATSON, J.S., EASTERLY, C.E., CANNON, J.B., TALBOT, J.B., Environmental Effects of Fusion Power Plants Part II: Tritium Effluents, (Submitted to Fusion Technology) ORNL, Oak Ridge, 1985

(4) Preliminary Assessment of Environmental and Safety Considerations in Siting the Tokamak Fusion Core Experiment, (internal report), Fusion Safety Program, INEL, 1983, EG&G Idaho

(5) PIET, J.S. Approaches to Achieving Inherently Safe Fusion Power Plants, (submitted to Fusion Technology), INEL, 1985, EG&G Idaho


(7) Requirements for the Safety Analysis of CANDU Nuclear Power Plants, AECB Canada Consultative Document C-6, June 1980

(8) KEMPE, T.F., RUSSELL, S.B., DONNELLY, K.J., Ontario Hydro, BRILLY, H.J. (EG&G Idaho), Conference Paper at Second Topical Meeting on Tritium Technology, Dayton, Ohio, April/May 1985

(9) NEIL, B.C.J., Summary of Environmental Radiological Data, Ontario Hydro Health and Safety Division Reports, Ontario Hydro, Toronto, Canada, 1984; 1985; 1986


BULK GETTERS FOR TRITIUM STORAGE

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ABSTRACT

A program was carried out to develop working experience with bulk metal getter beds for tritium handling in support of the Tritium Removal Facility being constructed at the Darlington Nuclear Generating Station site. This paper summarizes the data base developed for temporary and long-term tritium storage on bulk getters, namely the operating characteristics of a series of uranium and titanium beds built and tested during this program. Descriptions of doubly contained 3 kg and 25 g uranium beds, recently designed at Ontario Hydro for tritium service, is also presented.

INTRODUCTION

Tritium is produced in significant quantities in the CANada Deuterium Uranium (CANDU) reactors. The Tritium Removal Facility (TRF), which is in the advanced stages of construction at the Darlington Nuclear Generating Station, will extract tritium from tritiated heavy water. The removed tritium will be stored in Immobilization Tritium Containers in titanium "tritide". As demand dictates, tritium will be transferred from the long-term storage titanium beds to temporary storage uranium beds and subsequently metered into appropriate shipping containers.

A research program was formulated to cultivate working experience with bulk getter beds for tritium handling in support of the TRF. This paper is a summary of the data base developed during this program. Specifically, the paper presents the characteristic operating properties of uranium beds of various sizes: simple 15 g bed, doubly contained 6 kg bed, two-tier 660 g bed, and simple 100 g shipping bed. The performance of loading and unloading a simple 200 g titanium bed are also presented. Descriptions of doubly contained 3 kg and 25 g uranium beds, recently designed at Ontario Hydro for tritium service, are given.

URANIUM GETTER BEDS

The low dissociation pressure of uranium hydride at ambient temperature, 30 mPa at 25°C, and a dissociation pressure equal to atmospheric pressure at 432°C (2), essentially independent of composition, make uranium a very versatile getter for tritium service. The narrow operating temperature range of approximately 400°C permits rapid thermal cycling of uranium and thereby making it amenable for a number of swift applications: temporary storage; short, repeated recovery operations; and helium separation from tritium gas. These properties make uranium an ideal material for temporary storage of tritium.

Simple 15 g Bed

The loading and unloading rates for a simple 15 g uranium powder bed were studied on the process loop shown in Figure 1. (3)

![Figure 1: 15 g Uranium Test Bed Loop](image)

The factors influencing the hydriding rate of the bed are the bed over-pressure (bed pressure-equilibrium pressure), the initial bed capacity, the
bed temperature, the bed orientation, the helium content in the gas stream, and the in-line filter porosity. The rate of hydriding increases with increasing bed over-pressure and decreasing initial bed capacity (see Figure 2) and getter temperature. The charging rate is greater for a horizontally oriented bed than that positioned vertically; this is a reflection of greater heat transport from the hydriding region to the outside environment, which is a result of increased contact area between the uranium powder and the surface conducting heat to the external environment. The presence of helium in concentrations of 0.1% or greater noticeably reduces the rate of hydrogen uptake. Placement of filters with increasing tightness (decreasing porosity), for uranium particulate control, decreases the flow conductance and thereby limits the overall hydrogen uptake rate. Therefore, the choice of filter porosity must balance the need to control particulate migration into the loop with the overall rate of hydriding and dehydriding. It is worth noting that the uranium particulates are largely submicron in size and that the filtering action is electrostatic adhesion of the white flecks to the filter surface (see Figure 3). (3)

Hydrogen regeneration from the 15 g bed was achieved within 20 min at a temperature of 350°C. A total of 125 hydrogen loadings and unloadings were conducted without any noticeable degradation in the macroscopic performance of the bed.

**Doubly Contained 6 kg Bed**

The operating characteristics of a doubly contained 6 kg uranium bed, designed and built by Los Alamos Scientific Laboratory (LASL) for the Tritium Systems Test Assembly, were determined at the research laboratories of Ontario Hydro. A schematic of the process loop on which the uranium bed was tested is shown in Figure 4, and an expanded view of the LASL bed is shown in Figure 5. (4)

The factors that influence the loading rate of a large uranium bed, such as the LASL U bed, are those already identified in the previous section. However, given the increase in scale, some of the factors have a greater impact on the performance of the bed. Increase in the quantity of uranium permits more hydrogen loading, implying a greater production of heat. The geometry of the primary vessel vis-à-vis the uranium powder must be optimum to ensure good thermal transport. Furthermore, during loading the secondary volume must be filled with a high thermal conductivity gas for maximum heat transport. In short, the rate of loading is critically dependent on the balance between the rate of thermal transport from the uranium powder to the external environment, via the primary and secondary vessels, and the rate of heat liberation in the uranium powder due to the exothermic hydriding reaction.
The loading rate dependence on the initial hydrogen to metal ratio on the 6 kg LASL U bed is illustrated in Figure 6. The effect of the choice of gas in the secondary volume, or lack of it, during loading is shown in Figure 7; the concomitant change in temperature of the primary vessel is given in Figure 8. The higher thermal conductivity of hydrogen relative to helium results in a shorter charging period and a smaller increase in the primary vessel temperature. On the other hand, in the absence of a conducting fluid a significant reduction in the overall rate of loading and a corresponding increase in the primary vessel temperature are observed.

Heat transport again plays a major role in the rate of hydrogen regeneration. The critical factors are: intimate contact between heaters, the primary vessel wall, and the copper blocks containing the uranium powder; and, minimum thermal transport contact area between the primary and secondary containers. To recover hydrogen from the LASL bed required typically two days at a regeneration temperature of approximately 330°C. The recovery process was not carried out at a higher temperature due to the unavailability of a sufficient number of functional heaters on the primary container.

Two-Tier 660 g Bed

The two-tier, four-quadrant, 660 g uranium bed, designed, built and tested at Ontario Hydro, is shown in Figure 9.(5) The hydrogen loading behaviour of this bed was generally similar to the 15 g bed. The placement of a 5 μm filter between the tiers, in addition to the 5 μm filters at the inlet and outlet, resulted in restricting the flow and thereby limiting the overall rate of hydriding. Loading the uranium bed via both the inlet and outlet ports proceeds more rapidly. Essentially complete hydrogen recovery, from a fully loaded bed, at 375°C is achieved in 6 h.

Simple 100 g Shipping Bed

The Amersham International Transportable 100 g uranium bed (6) was examined for its operating characteristics on a test loop similar to that shown in Figure 1. The uranium is contained within a stainless steel cylindrical vessel outfitted with a 70 mesh filter (~180 μm), a single inlet/outlet port, and a Cajon valve. The uranium bed, during transportation, is packed in a stainless steel, O-ring sealed, vacuum tight canister. The canister in
turn is placed amidst supportive, thick cork insulation within a galvanized steel shipping drum.

Loading the uranium bed to capacity in hydrogen to metal ratio increments of 0.78 requires approximately 15 min. A reduction in the rate of hydrogen loading in the presence of helium was similar to that observed with the 15 g bed. At a temperature greater than 350°C, the maximum rated storage capacity of the uranium bed (20 kJ equivalent hydrogen) is essentially recovered within 0.5 h.

TITANIUM GETTER BED

Titanium is a vigorous hydride former with a very low equilibrium pressure at ambient temperature. A dissociation pressure equal to atmospheric pressure is achieved at a temperature of approximately 650°C and hydrogen to metal ratio of unity. (7) Thus, titanium has been chosen as the primary or long-term storage medium at the TRF.

Simple 200 g bed

A simple 200 g titanium bed, one quarter the scale of an Immobilization Tritium Container, was designed, constructed and tested. (5) The bed consisted of 200 g of titanium sponge contained within a stainless steel cylindrical chamber outfitted with 5 μm filters and an inlet and outlet port on either side of the vessel.

Hydrogen charging of titanium proceeded very rapidly. The system pressure dropped from an atmosphere to 100 Pa, with a hydrogen to metal increment upon loading of 0.25, within 100 s, largely independent of the bed temperature and composition (upto hydrogen to titanium ratio, H/Ti-1). Essentially complete hydrogen unloading into the vacuum system, at approximately 550°C was achieved within 18 h (see Figure 10).

Numerous hydrogen transfers from the titanium bed to the 660 g uranium bed were conducted to simulate tritium transfer from a permanent storage medium to a temporary one. A direct, pump-less transfer (with initial H/Ti-1.2, regeneration temperature of 550°C, and an empty uranium bed at ambient temperature) required 18 h to transfer 83% of the hydrogen. Transfers using a Normetex circulation pump (circulating through both beds) required 19 h to transfer essentially all the hydrogen, in the absence and presence of helium.

DOUBLY CONTAINED URAMIUM BEDS FOR TRITIUM SERVICE

A 500 kCi (18 500 TBq) uranium storage bed with secondary containment, shown in Figure 11, has been designed and built for tritium service at the TRF. At the time of writing the operating characteristics of the storage bed were being determined. The stainless steel primary vessel contains 3 kg of uranium partitioned within a two-tier, six-sextant copper block. The two-tiers are separated by a 100 μm stainless steel sintered porous filter. Loss of uranium particulates is controlled by the use of 5 μm filters stacked onto the copper block. The bed can be heated with the aid of band heaters placed on the primary vessel. The secondary vessel is made out of stainless steel outfitted with a port to permit evacuation of the secondary volume and penetrations for electrical and thermocouple feed-throughs. The primary vessel is surrounded by a number of dimpled stainless steel foils, which serve to minimize radiative heat loss during unloadings.

![Diagram of a doubly contained uranium bed](image-url)
advanced stages of fabrication. The primary chamber contains 25 g of uranium with 5 μm sintered filters welded in place. The primary vessel is suspended from the weldments between the inlet and outlet ports and the secondary vessel. A band heater on the primary vessel will be used to heat the uranium. The secondary vessel is outfitted with penetrations for electrical and thermocouple feed-throughs and a flow port to permit evacuation of the secondary volume.

FIGURE 12: 5 kCi (185 TBq) DOUBLY CONTAINED URANIUM BED

CONCLUDING REMARKS

Uranium and titanium storage beds have good operating characteristics for application as temporary and permanent tritium storage media. Uranium, however, has certain drawbacks: it is a pyrophoric material, chemically toxic and therefore requires particulate control, and it is a controlled (nuclear) material. Currently work is underway to identify getter materials that are potential alternates to uranium with the following desired characteristics: dissociation pressure of 10 mPa and 0.1 Mpa at 20°C and 400°C, respectively; negligible to modest expansion during hydriding, thereby reducing the pyrophoricity and particulate control concerns; good thermal conductivity; rapid hydriding and dehydriding kinetics; tolerant to impurities; stable in air; easy activation procedure; and minimum helium retention.

REFERENCES


ANSTRACT

The Chalk River Nuclear Laboratories operate a special laboratory capable of handling pure tritium as T. This facility is unique in Canada, and is available for carrying out a variety of scientific and technical work with concentrated tritium. It is used for both AECL-funded work and for contract work for those researchers outside of AECL who want to do investigations with tritium, but do not have their own facilities. The current inventory limit is 10 000 Ci.* The laboratory was established to demonstrate the safe packaging of pure T, that will be produced by the Chalk River Tritium Extraction Plant scheduled to begin operation in early 1987. The laboratory is now being used to assist in the design and evaluation of components for the Tritium Extraction Plant and for contract work, primarily to support the Canadian Fusion Fuels Technology Project.

INTRODUCTION

The tritium laboratory is an integral part of the CRNL tritium technology program which encompasses the basic research on the health physics of tritium, development of tritium measuring instrumentation, the behavior of tritium in the environment, biological effects of tritium, the building of a tritium extraction plant for AECL’s research reactors and tritium related fusion programs. The tritium laboratory is a facility which can contribute to the experimental aspects of these programs, through its ability to handle large quantities of pure tritium.

The laboratory was established in 1977 to develop a method to safely package tritium recovered from FRNTR reactors in a form suitable for long term storage, as a waste product. A secondary objective was to conserve expertise in the technology of handling concentrated tritium that would be necessary for the operation of tritium recovery plants, committed by Ontario Hydro and by AECL.

DESCRIPTION

The laboratory is a 10 x 7 m room equipped with eight fume hoods and a 2.5 m³ inert atmosphere glove box. The room is independently ventilated to the building roof at a rate of approximately 180 m³/min to provide 48 air changes per hour. This ventilation rate was selected from previous use and was not expressly installed for the tritium application. A maximum of 20 000 curies of tritium in the elemental form is the limit set by the AECL Nuclear Safety Advisory Committee for this facility. There is no air decontamination system installed. In the event of a tritium spill into the room, it would be exhausted to the roof vent. Release of the total inventory of tritium to the ventilation system would deliver a dose commitment of 9 mrem* to persons standing at the opening of the exhaust vent.

The key safety philosophy is the prevention of tritium release through multiple barrier containment. The inert atmosphere glove box provides secondary containment for all the operations with concentrated tritium. Equipment associated with the operation of the inert atmosphere glove box and the tritium handling apparatus is housed in a second glove box with an air atmosphere, located below the main box. The arrangement is shown schematically in Figure 1. A separate inert atmosphere glove box is provided for maintenance and repair of tritium contaminated equipment.

FIG. 1 - TRITIDING APPARATUS IN GLOVE BOX

* 1 Ci = 37 GBq

The 2.5 m³ inert atmosphere glove box is a high integrity stainless steel enclosure with polycarbonate windows, an evacuable transfer port and an argon gas circulation and purification system. The argon is circulated at 0.5 m³/min through two hot titanium getter beds for the control of O₂ and CO₂ (<1 µL/L), H₂O (<5 µg/g), and tritium (5-20 mCi/m³). On-line analytical instrumentation is provided to monitor the quality of the argon atmosphere. The argon in the box is maintained at a slight positive pressure of 10⁻³ Pa in order to prevent ingress of air. Operation at positive pressure required a leak tight box to prevent tritium ingress into the room. The leak rate has been measured with Argon to be 0.007% of the box volume/hour.

The tritium handling apparatus inside the glove box is an all metal vacuum system with bellows sealed valves, and all metal demountable fittings. The pumping system consists of an oil vapor diffusion pump backed by a mechanical rotary pump capable of maintaining a pressure of 10⁻³ Pa. The vacuum pumps are located in the lower air atmosphere glove box. While oil containing pumps are sometimes not recommended for use in tritium service because of...
Tritium contamination of the oil and subsequent exposure to operating personnel, this has not been a problem in our experience. In normal operation these pumps are exposed only to very small quantities of tritium. Special provisions are made to change the oil in pumps without exposure to the operator. Typical tritium concentrations observed in pump oil have been in the range 20-400 mCi/mL. Included in the vacuum apparatus is a special circulation pump designed for tritium service. This pump operates from 6 Pa on the suction side to 500 kPa on the discharge side. A reversible motor allows pumping in both directions. These features are advantageous in moving gas from one section of the apparatus to the other. Also attached to the tritium handling apparatus are a number of specialized components, which include a calibrated gas volume, a gas chromatograph, a mass spectrometer, a high range ionization chamber, and a number of uranium and titanium metal getter beds. The calibrated volume, along with pressure and temperature measurements allow the determination of the quantity of gas. The gas chromatograph and/or mass spectrometer allow determination of the isotopic composition of the gas.

An extensive tritium monitoring system is in place. Instruments are installed to continuously monitor the room air, the air discharged from the building vent, and the air exhausted from the lower glove box. These measurements are made to monitor the safety of the room atmosphere, and to determine tritium releases from laboratory operations. The argon atmosphere inside the glove box is continuously monitored to warn of unsafe working levels in the box and to detect leaks from the tritium handling apparatus. A gas sample from the transfer pass-through box is passed through an ionization chamber to ensure material removed from the box is not contaminated with tritium before it is taken out into the room. A separate intermediate range instrument is installed in the argon glove box in the event of a tritium leak into the glove box which would put the normal monitor off scale. In addition to the fixed monitors a number of portable instruments are used as required. The Instruments are all of the ionization chamber type with the sample drawn through a particulate filter and an ion trap into the ion chamber and then returned to the source. Most of the instruments are commercially available, with the exception of the high and intermediate range ion chambers and the stack monitor which were built in-house. These instruments measure total tritium, i.e., both the water and hydrogen forms. A prototype monitor developed by McElroy et al. (1,2) which discriminates between the two forms by separating them using a Nafion membrane, is installed for evaluation purposes. It has been used to monitor the room, the building vent and the lower glove box exhaust.

The laboratory also contains a scintillation counter used for the determination of tritium concentration in liquids; primarily for analysis of oil samples and water samples. Water samples arise from the oxidation of hydrogen samples containing small amounts of tritium, as part of an analytical procedure to determine the tritium concentration of the gas.

Tritium contamination on surfaces is estimated by swiping a 100 cm\(^2\) area, equilibrating the swipe in water, and analysing the water for tritium by scintillation counting.

A computer based system for accounting of the tritium in the laboratory has been developed (3) to record how much tritium is in the laboratory and where it is located. Possible locations include a number of metal hydride beds or gas containers, located within the argon atmosphere glove box, that are used for either storage or experimental purposes. Transfers of tritium between beds is recorded in the computer system. At any time the computer can be instructed to provide a summary of the amount and composition of tritium contained in each bed. The program calculates the decay of tritium and adjusts the inventory estimate accordingly. Physical inventories are carried out periodically, where the entire amount of gas on a bed is desorbed and its composition measured for comparison with the inventory calculated.

TRITIUM LABORATORY PROJECTS

Tritium Storage and Packaging

Methods for the long term storage of tritium (150 years) were reviewed and the use of metal hydrides chosen for detailed investigation. While a large number of metal hydrides exist, only a relatively small number of these meet the requirements of being easily prepared, having a low dissociation pressure of tritium and being stable at storage conditions. The basic reaction for hydride formation is,

\[ \text{Metal (M)} + \frac{x}{2} \text{H}_2 \rightarrow \text{MH}_x \]

Because this reaction has to be carried out with pure \( T_2 \) in a glove box, it is important that the formation take place at low temperature in a simple, single step reaction. The hydrides of zirconium, titanium, erbium and yttrium all meet the requirements of very low dissociation pressures which at 25°C are all \(<10^{-20}\) atmospheres. At elevated temperatures between 500 and 1000°C, the titanium and zirconium become completely dissociated whereas the erbium and yttrium remain in the hydride form. Because \( T_2 \) is a material with potential value, it is important to choose a storage medium that is both safe for the long term, and provides the possibility of recovering the tritium. This consideration has led to the choice of titanium sponge as the storage medium. The work on titanium hydride (4,5,6) has shown it could be readily formed by reaction of titanium in the sponge form that had been vacuum annealed by heating to \( >800^\circ\text{C} \) for two hours with pure hydrogen at room temperature. The reaction is rapid and complete in less than five minutes, provided the hydrogen is pure. Contaminants such as air or helium inhibit the reaction. With tritium, helium-3 is always present so that provision for circulating the gas through the metal bed is required to overcome the gas blanketing of the metal.

The resistance of the metal tritide to leaching has been extensively studied (7) and it has been shown the cumulative fractional release is \(<10^{-4}\) over a 100 day period. Measurements on the stability of the metal hydrides in air has also shown that the hydride will not burn in air until it reaches a temperature of \( >900^\circ\text{C} \) (8). The ignition temperature is a function of the particle size of the material. An ignition temperature of \( <400^\circ\text{C} \) has been measured on the finest particle size fraction (30 \( \mu\text{m} \), found after multiple hydriding-dehydriding cycles.

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The titanium tritide was prepared in a reactor that also serves as its primary storage vessel. This is a 6 L stainless steel container, shown in Figure 2. The vessel is designed to contain 850 g of titanium sponge, which, when loaded to an average of TiT,*, will package 50 g or 0.5 MCI per package. The 6 L volume was dictated by the need to contain the helium-3 formed from decay of the tritium. For the transportation package, the primary storage vessel is placed in a secondary stainless steel vessel and the combined assembly placed in an insulated drum. The tritium laboratory has been used to demonstrate the packaging of pure tritium in such a package. This program was completed in March 1985.

**FIG. 2**

**TRITIDE TRANSPORTATION PACKAGE**

The laboratory has been used to prepare a number of gas mixtures containing tritium, from the mCi/m³ to the $\mu$Ci/m³ range, for use by other researchers in a variety of projects. These have included mixtures of HT in H₂ for tritium density measurements, HT in Ar for tritium monitoring of the tritium or for implantation into single crystals of Zr, T, in H₂ for thermodynamic studies in water radiolysis, and the contamination of metal surfaces with HT for skin uptake studies.

**Preparation of Tritium Containing Gas Mixtures**

A contract project for the Commercial Fusion Fuels Technology Project (CFFTP) has been carried out in the laboratory. A small fusion fuel cleanup system has been built and tested for its ability to purify a stream of hydrogen with impurities simulating those from a fusion reactor (12). The equipment has demonstrated effective removal of O₂, H₂O, CO, CO₂, N₂, H₂, and CH₄, using either two uranium beds at different temperatures or a combination of a uranium bed and a zeonex getter. Phase II of the project is now underway where the cleanup system will be installed in a new inert atmosphere glove box in the tritium laboratory. When installed, testing with tritium will be done.

**Accounting of Tritium in Facilities**

Accounting of tritium in facilities such as tritium extraction plants and laboratories is necessary, and to do this accounting one must be able to analyze the tritium content of a gas stream. Three methods for hydrogen isotopic analysis, based on previous work in the literature, gas chromatography (9), mass spectrometry, and ionization chambers (10), have been employed in the tritium laboratory. Of these, the simplest is the ionization chamber, which measures total tritium, and is used routinely. Gas chromatography is used as the major technique for complete isotopic analysis, which will separate all six hydrogen isotopic species as well as helium, for accurate analysis it is necessary to have accurate calibration factors. It has been observed in the work in the tritium laboratory that the sensitivity for six species are markedly different using a thermal conductivity detector, for example the sensitivity to T is 2.5 lower than to H₂. Major errors would be introduced if the same value were used for each of the hydrogens. Gas chromatography will be used in the Chalk River Tritium Extraction Plant.

**Preparation of a Primary Standard**

A contract project for a commercial supplier of tritium instrumentation was carried out to prepare a certified gas mixture containing 1 Ci/m³ of tritium in argon. It was a requirement that the gas mixture was prepared using equipment traceable to National standards. This was done using carefully calibrated gas measuring equipment in the inert atmosphere glove box along with special precautions to prevent loss of tritium to the walls of the container. Two gas bottles, each containing approximately 2000 LSTK of standard gas, were prepared. The composition of the gas calculated from the measurements of quantities used in preparation was 0.9% and 0.99 Ci/m³ from weight and pressure measurements, respectively, for standard #1, and 1.07 and 1.13 Ci/m³ for standard #2. After preparation samples of each of these standards were oxidized to water for tritium analysis by liquid scintillation counting. The results were 1.02 Ci/m³ for standard #1 and 1.11 Ci/m³ for standard #2, both in good agreement with the composition by preparation. This agreement also demonstrated the tritium in the standard was not adsorbed on the walls of the container. After delivery of the standard, the customer observed a higher value for the tritium concentration than we had reported. This discrepancy was traced to a transfer by the customer of an aliquot of the standard to a container that had been previously contaminated with tritium.

**Testing and Calibration of Process Tritium Analyzers**

As part of the tritium monitoring program at CHNL, three models of process instruments have been designed and built to provide on-line analysis of process streams in the Tritium Extraction Plant and for the final T₂ product analysis. These range from 0.01 Ci/m³ to 2.5 x 10⁻⁶ Ci/m³ (100% T₂). These prototype monitors have been tested and calibrated over this concentration range in the tritium laboratory and shown to meet the process requirements.

**Recovery of HT from UH**

A uranium bed was designed and built to recover HT produced by the neutron irradiation of UH in the FRX power cycling loop. This bed provided a clean up of the helium for the loop user and provided a source of concentrated tritium for use in the laboratory. In addition to the recovery from UH, a laboratory program was also carried out to examine the gettering of hydrogen from inert gas streams in detail (11).
The second major fusion project being carried out in the laboratory is the extraction of tritium from potential tritium breeder materials that have been irradiated with neutrons in the CRNL reactors. The quantity of tritium, its chemical form and its release rate have been determined for a number of lithium ceramic materials. This is part of the major CFFTP-AECL fusion breeder blanket program being carried out at CRNL (13).

Tritium Safe Handling Courses

Through a contract with CFFTP the tritium laboratory provides a practical hands-on section of a week-long course that is held in Toronto and Chalk River. In this practical session each participant does work in the glove box with tritium containing gas. In addition to the session in the tritium laboratory, practicals are held in tritium monitoring, dosimetry and environmental measurements. Four courses have been held to date with the fifth scheduled for October 1986.

SERVICES AVAILABLE

This laboratory is a unique resource in Canada, in that it provides a facility that is capable of doing a wide variety of work with large quantities of tritium, from dilute concentrations to pure T₃, in a location that has all of the necessary support services. These include radiation and health safety groups, tritium monitoring expertise, bioassay services, workshops, and expertise in a number of technical fields, such as physics, chemistry, mathematics, computer science, materials, engineering, and design. In addition the laboratory is staffed by a competent team of experienced people.

Consulting is provided on the design and building of tritium handling facilities. This has been done for the new 1200 CI capacity laboratory being built by Ontario Hydro Research, and the much smaller laboratory at the University of Toronto. Through CFFTP, consultation is also being provided for major tritium laboratories planned in Germany and Italy.

The glove box and tritium container technology developed in the course of the tritium packaging project have been licensed for manufacture to Numet Engineering in Peterborough.

Other work that has been explored is the labeling of molecules for tritium tracer work in scientific and industrial applications, and the preparation of tritiated polymers for use in self-powered lighting applications.

ACKNOWLEDGEMENT

Major contributions to the development and operation of the tritium laboratory have been made by R.E. Johnson, S.R. Bokwa, C.M. Shultz, F.B. Gravelle and R.J. Keyes.

REFERENCES

SESSION H: THERMOHYDRAULICS II

Chairman: D.B. Primeau, AECL CANDU Ops.

Analytical and experimental studies in support of fuel channel critical power improvements.

The onset of subcooled nucleate boiling in nuclear fuel bundles.
C.W. Snoek, AECL CRNL

Circumferential drypatch spreading on a simulated CANDU fuel string with non-uniform axial heat flux.
C.W. Snoek, AECL CRNL

Prediction of void fraction in steady horizontal stratified flow.
P. Gulshani, AECL CANDU Ops.

Air-water flooding in a 90° elbow with a slightly inclined lower leg.
P.T. Wan and V.S. Krishnan, AECL WNRE

Numerical simulation of a confined jet under suction and counter-momentum for the Canadian MAPLE Research Reactor.
S.Y. Shim and D.K. Baxter, AECL WNRE
ANALYTICAL AND EXPERIMENTAL STUDIES IN SUPPORT OF FUEL CHANNEL CRITICAL POWER IMPROVEMENTS


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ABSTRACT

Various methods for increasing the CHF in 37-element bundles have been examined. The most promising techniques have been assessed by considering their effect on critical channel power (CCP) and critical heat flux (CHF). It is concluded that techniques for increasing CHF are available which could be used in current fuel bundles to provide an additional margin to dryout.

INTRODUCTION

The design power output of nuclear fuel bundles is limited either by fuel centre melting or by high sheath temperatures. High sheath temperatures which may cause significant element low or excessive corrosion, can be avoided by operating at heat flux levels well below the CHF or by operating at conditions where the post-CHF heat transfer is reasonably effective in keeping the fuel sheath temperature within acceptable limits. It is common practice in the thermal design of water-cooled reactors to avoid CHF. In CANDU reactors, avoidance of CHF is sufficient but not necessary for the ready demonstration of the prevention of pressure tube damage due to Loss-of-Regulation Accidents.

During the past 10 years, Chalk River Nuclear Laboratories (CRNL) have investigated the CHF, pressure drop and post-CHF performance of various bundle designs. One of the main objectives of these studies is to increase the fuel bundle critical heat flux without severe penalties of increased channel pressure drop. The results have demonstrated a significant potential for increasing CHF. An assessment of the increase in CCP has also been made for certain bundle designs by including the effects of bundle pressure drop and hydraulic characteristic of the complete reactor circuit. The effects of a change in flow and CHF on the CCP can best be illustrated by considering a channel power versus flow diagram as shown in Figure 1. The top curve represents the hydraulic characteristic of the reactor channel and is governed by the system hydraulics and pump characteristic while the bottom curve presents the unique relationship between flow and dryout power for a given bundle geometry, inlet temperature and pressure. The intersection between these two curves defines the CCP. The following sections will describe how CHF enhancement, flow, dryout definition, etc. will affect the CCP.

![Fig. 1: Schematic of Flow and CHF Characteristics of Critical Channel.](image-url)
INCREASE CCP BY USING CHF ENHANCEMENT TECHNIQUES

General

CRNL has recently completed a series of tests on 37-element bundles equipped with various types of CHF enhancement techniques. The objective of these tests was to examine the potential for increasing the CHF using several techniques with a minimum penalty in increased pressure drop. Figure 2 shows schematically the effect of introducing a CHF enhancement technique on channel flow and CCP. In general, CHF enhancement techniques increase the hydraulic resistance and hence decrease the channel flow which by itself would also decrease the CCP (the CCP decreases when going from A to B in Figure 2). However, the enhancement in CHF shifts the CHF curve to the right, thus compensating for the negative effect of an increased hydraulic resistance and resulting in a net gain in CCP (C on Figure 2).

Experiments

A series of experiments was conducted at CRNL to evaluate techniques for enhancing the CHF in CANDU fuel bundles. The following is a brief description of the experiments and their results.

The techniques were evaluated using a simulated, full scale, six-metre long, 37-element CANDU fuel channel installed in the MR-3 Freon modelling loop. The 37-element test assembly was electrically heated with uniform axial and depressed radial heat flux. Dryout power (bundle power at first occurrence of CHF), pressure drop, and post-dryout temperatures were measured. The values were compared with previously obtained values for the reference 37-element bundle.

Techniques examined in this series of CHF enhancement studies included:
- two additional planes of bearing pads,
- no bearing pads,
- two additional planes of spacers and bearing pads (Fig. 3),
- four additional planes of spacers and bearing pads (Fig. 4),
- two planes of vortex generators,
- two planes of flow obstruction vanes,
- two grid spacers (at 1/3 and 2/3 bundle length positions) (Fig. 5),
- two grid spacers (at mid-bundle and end plate bundle positions),
- centre rod removed (36-element bundle), and
- 37-36-element bundles in alternate order.

Details of the CHF enhancement techniques and their results are provided by Macdonald (1985 and 1986).

The basic test bundle was designed to be geometrically identical to a CANDU PHW-750 MWe fuel channel containing twelve 37-element bundles with the individual rods of each 50 cm long bundle aligned. The heated lengths were made of thin wall Inconel tubing. The wall thickness was sized to have the overall bundle resistance match the power supply and produce the desired radial heat flux depression. Copper spool pieces were used to connect the tubes at the bundle segmentations. Simulated end plates were fitted at the bundle junctions.

The test section into which the test assembly was installed was essentially a 15.2 cm diameter pipe with a fibreglass-epoxy liner. The liner, having an inside diameter of 10.34 cm, simulated a CANDU pressure tube and provided electrical insulation between the bundle and the pipe.

The test assembly was instrumented with thermocouples and resistance temperature detectors (RTD's) for detecting CHF. First occurrence of CHF was detected by an element thermocouple reading increase of 5 K or an RTD signal increase of 0.8 mV while the bundle power was being increased a small amount.

The CHF measurements for each enhancement technique were compared to a best-fit CHF correlation for the reference bundle data. Pressure drop comparisons of the enhancement techniques were made by calculating the obstruction pressure loss coefficient from the single phase pressure drop data.

Table 1 summarizes the effects of the CHF enhancement techniques on the CHF, pressure drop, and post-dryout behaviour. Significant increases in CHF were observed by using additional spacer planes (see Figures 3 & 4) or by installing grid spacers instead of the conventional CANDU split spacers (Figure 5). The maximum observed increase in CHF was 139% (constant dryout quality) for the two grid spacer
Table 1

<table>
<thead>
<tr>
<th>Enhancement Technique</th>
<th>Effect on CHF</th>
<th>Effect on $\Delta P$\textsuperscript{1,2}</th>
<th>Effect on $h_{PD0,\text{min}}^\circ$ for 6-m Freon test bundle (Calculated using average of $K_{F,\text{F,skin}}$ values for other 37-element bundles). $K_{F,\text{F,ob}}$ = flow obstruction coefficient for the entire 6-m bundle string, $f$ = bundle sheath friction factor.</th>
<th>$K_{F,\text{F,ob}}$ for 6-m fully aligned Freon test bundle = 6.90. For string of randomly misaligned CANDU PHW-750 MWe bundles, $K_{F,\text{ob}} = 8.33$.</th>
<th>Extrapolated for full 6 m length to be identical to downstream end.</th>
</tr>
</thead>
<tbody>
<tr>
<td>(Change per 50-cm bundle)</td>
<td>$x_{DO} = ^\circ C$</td>
<td>$\Delta H_{in} = ^\circ C$</td>
<td>Increase in $K_{F,\text{F,ob}}$</td>
<td>Increase in $\Delta P_{frlc.}$</td>
<td>$G=C=\frac{C}{X_{PD0} = ^\circ C}$</td>
</tr>
<tr>
<td>Two Additional Bearing Pad Planes</td>
<td>Ave. 8.0</td>
<td>Ave. 3.0</td>
<td>1.7 to 10.6</td>
<td>13.6</td>
<td>Increase of 10 to 40</td>
</tr>
<tr>
<td>No Bearing Pad Plane</td>
<td>Ave. -30.0</td>
<td>Ave. -10.0</td>
<td>21.0 to 2.1</td>
<td>2.99</td>
<td>Increase of 0 to 20</td>
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<tr>
<td>Two Additional Spacer &amp; Bearing Pad Planes</td>
<td>Ave. 42.5</td>
<td>Ave. 12.5</td>
<td>5 to 21</td>
<td>13.6</td>
<td>Increase of 10 to 40</td>
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<tr>
<td>Four Additional Spacer &amp; Bearing Pad Planes</td>
<td>Ave. 50.2</td>
<td>Ave. 14.1</td>
<td>9 to 20</td>
<td>23.6</td>
<td>Increase of 0 to 20</td>
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<tr>
<td>Two Planes of Vortex Generators</td>
<td>Ave. -0.5</td>
<td>Ave. -0.6</td>
<td>-6.5 to 7.9</td>
<td>h.6</td>
<td>Varied from decrease of 20 to increase of 15</td>
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<tr>
<td>Two Planes of Flow Obstruction Vanes</td>
<td>Ave. 3.1</td>
<td>Ave. 0.8</td>
<td>-5.7 to 8.5</td>
<td>5.79</td>
<td>Increase of 5 to 20</td>
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<tr>
<td>Two Grid Spacers (at 1/3 and 2/3 bundle length positions)</td>
<td>Ave. 59.3</td>
<td>Ave. 15.5</td>
<td>9.6 to 25.4</td>
<td>99.0</td>
<td>Increase of 0 to 10</td>
</tr>
<tr>
<td>Two Grid Spacers (at 1/2 and end plate bundle positions)</td>
<td>Ave. 5.8</td>
<td>Ave. 1.0</td>
<td>-2.4 to 6.7</td>
<td>22.06</td>
<td>103.0 \textsuperscript{3}</td>
</tr>
<tr>
<td>Centre Rod Removed (36-element bundle)*</td>
<td>Ave. -16.9</td>
<td>Ave. -7.3</td>
<td>-13.0 to 3.0</td>
<td>-11.1</td>
<td>Increase of -15 to 15 on outer rods. Decrease of 5 to 10 on inner rods.</td>
</tr>
<tr>
<td>37-36-Element Bundles in Alternate Order$^5,6$</td>
<td>Ave. -9.2</td>
<td>Ave. -3.8</td>
<td>-8.6 to 3.8</td>
<td>-1.20 \textsuperscript{3}</td>
<td>-5.6 \textsuperscript{3}</td>
</tr>
</tbody>
</table>

1 $K_{F,\text{F,ob}}$ = $K_{F,\text{F,skin}}$ for 6-m Freon test bundle = 21.52 (Calculated using average of $K_{F,\text{F,skin}}$ (fl/layer) values for other 37-element bundles). $K_{F,\text{ob}}$ = flow obstruction coefficient for the entire 6-m bundle string, $f$ = bundle sheath friction factor.

2 $K_{F,\text{ob}}$ for 6-m fully aligned Freon test bundle = 6.90. For string of randomly misaligned CANDU PHW-750 MWe bundles, $K_{F,\text{ob}} = 8.33$.

3 Extrapolated for full 6 m length to be identical to downstream end.

* Actual flow area and hydraulic equivalent diameter were used in pressure drop calculations.

5 The 6-m-long bundle consisted of six 36-element bundles followed by three 37-element bundles and three 36-element bundles in alternating order. The centre rod of the 37-element bundles was unheated.

6 For effect on CHF, heat flux was converted to 37-element bundle, i.e., heat flux = power/heated area of 37-element bundle.
cases; the corresponding increase in friction pressure drop is approximately 100%. In all cases an increase in CHF also resulted in an increase in post-dryout heat transfer. This was expected as most CHF enhancement devices also increase the turbulence in the flow, thus improving the wall-liquid interaction.

Assessment of CHF Enhancement Results

The observed average increase in CHF and bundle flow resistance from Table 1 were used in the NUCIRC code to calculate the effect on CCP if these enhancement techniques were introduced in an existing reactor. It was assumed that the reported relative increase in CHF and pressure drop, based on experiments on 37-element bundles cooled by Freon, may be applied to water. Appendix I discusses the validity of this assumption in detail. A sensitivity analysis was first performed to determine the change in CCP due to a variation in any of the thermohydraulic characteristics. The reference conditions used were: $T_{10} = 255^\circ\text{C}$, and $P = 10.6$ MPa and using a typical CANDU PHW-750 MWe critical channel.

EFFECT OF AN INCREASE IN FLOW ON CCP

In general, an increase in nominal flow will have a positive effect on the CCP as is illustrated schematically in Figure 6. This method for increasing CCP, easily obtainable in new reactor types by increasing the pump capacity, can only be applied to a limited extent in existing reactors (plausible options include (i) small increase in pump capacity by pump impeller modification, and (ii) reducing the hydraulic resistance of fuel bundles by rounding fuel endcaps etc.).

Current CHF correlations for 37-element bundles have a limited data base (the mass flux range was limited to $4.7 \text{ kg.m}^{-2}.\text{s}^{-1}$ which corresponds to the current critical channel flow at postulated dryout conditions). In this study they have been extrapolated to higher mass fluxes. Justification for this extrapolation came from the Freon bundle CHF results, obtained over a much wider range of flow conditions, which show the same trend of the extrapolated water correlation and may be extrapolated to water using established CHF modelling relationships (Appendix I). The results of our analysis show that an increase in flow of 15% would increase the CCP by
approximately 0.76%. The main drawback against a large increase in flow, (aside from the expense associated with an increased pumping capacity) is that it may eventually lead to significant bundle vibration and pressure tube fretting and thus may possibly require fuel bundle modification.

INCREASE CCP BY REDEFINITION OF DRYOUT

Dryout Behaviour

In general when a heated surface dries out in forced convective boiling, the surface heat transfer deteriorates and the surface temperature increases. The amount of temperature increase depends on the flow conditions and the surface heat flux. Therefore, to decide whether dryout is to be taken as the limiting criterion for the determination of the critical power, the dryout behaviour at various flow conditions must be considered.

Experimental studies of flow boiling in Freon-12 (Groenoveld, 1972) and water (Borodin, 1985, Era, 1967) have shown that the types of dryout observed can vary drastically. The types of dryout discussed below have been observed in electrically heated tubes and bundles.

1) Fast dryout (Figure 7a) is characterized by a sudden very large increase in surface temperature. In CHF experiments, fast dryouts usually activate high temperature trips, tripping the power supply and thus preventing a physical "burnout" of the heater. Fast dryout is usually observed at very high heat flux values and subcooled conditions where the mechanism of dryout corresponds to that of departure from nucleate boiling (DNB).

2) Stable Dryout (Figure 7b) is characterized by a sudden but moderate rise in surface temperature. This type of dryout occurs at conditions typical of the annular flow regime. The combined effect of evaporation and net entrainment slowly depletes the liquid film and eventually results in a liquid film breakdown or a film dryout.

3) Unstable Dryout (Figure 7c) does not provide a well defined dryout point. Instead a dryout region, characterized by fluctuating temperatures, corresponding to the appearance and disappearance of dry patches, is observed. Unstable dryouts generally occur at qualities higher than those of stable dryouts.

4) Slow Dryout (Figure 7d, 8) is not accompanied by the usual dryout temperature excursion; instead, a gradual increase of surface temperature with increasing power is observed. Slow dryouts are often difficult to detect because of the absence of a dryout temperature excursion. Figure 8 shows an example of a slow dryout thermocouple signal in water. Slow dryouts are usually observed at high mass velocities (greater than 3.0 Mg.m⁻².s⁻¹ in Freon-12 and water).

Fig. 7: Examples of Thermocouple Traces Obtained in Freon-12 (% Indicates Increase in Test Section Power).

Fig. 8: Example of Slow Dryout Obtained in Water in 37-Rod Bundle.

Onset of Intermittent Dryout (OID) and Onset of Dry Sheath (ODS)

The classical view of the dryout heat flux is that of a heat flux limit, which results in a steam blanketing of the heated surface. In the past, this has been assumed equal to the heat flux at the departure from nucleate boiling. This assumption is valid for stable and fast dryouts, usually obtained at low flows and/or subcooled or low quality conditions. However, for slow dryouts these two heat flux limits can differ significantly as is illustrated in Figure 9. The region between the two heat flux limits corresponds to the transition boiling regime. It may be viewed as an intermittent dryout region: here the heated surface temperature is too high to maintain continuous liquid contact but too low to maintain
continuous vapour contact. The intermittent dryout region is bounded by the onset of intermittent dryout \( T_{\text{OD}} = q_{\text{OD}} \) as the lower temperature and heat flux boundary and the onset of dry sheath heat flux boundary and the onset of dry sheath \( T_{\text{ODS}} = q_{\text{ODS}} \) as the upper temperature and heat flux boundary.

Figure 9 illustrates the position of the ODS and OID conditions for stable and slow dryouts. Note that for a stable (and fast) dryout \( q_{\text{OD}} > q_{\text{ODS}} \) and hence the transition boiling curve region is skipped once the CHF is exceeded. At a higher flow and/or coolant enthalpy, the post-CHF heat transfer is much more efficient and consequently the slope of the transition boiling changes to a positive value. This eliminates the dryout temperature excursion into film boiling.

\( q_{\text{ODS}} \) can be obtained directly from experimental data, as it equals the heat flux at which the surface temperature first exceeds the minimum film boiling temperature, \( T_{\text{min}} \). \( T_{\text{min}} \) may be obtained from empirical correlations: for the range of interest \( T_{\text{min}} \) is approximately 375°C (Groeneveld, 1986).

Currently we are investigating more direct methods of measuring \( q_{\text{ODS}} \). Ideally we need a direct measurement of the heat flux corresponding to the first continuous dry spot i.e. the first continuous indication of a 100% local wall voidage. Several methods for detecting wall voidage have been considered: tomography, optical probes, visualisation, ultrasonics. The ultrasonic method was chosen because it was successfully used to identify wet and dry regions inside a heated tube at atmospheric pressure and temperatures below 120°C. The ultrasonic method had to be modified and improved for installation on a heated tube, operating at temperatures up to 400°C and internal pressures up to 11 MPa.

Figure 10 shows schematically the current experimental set-up. Since suitable ultrasonic transducers that can operate at temperatures up to 400°C are unavailable, a MACOR standoff/delay line was designed. A silicon based ultrasonic coupling fluid was used to couple the 5 MHz transducer to the MACOR delay line, while the MACOR was bonded permanently to the Inconel heated surface using a ceramic paste. The transducer is used in a pulse-echo mode at 5 MHz. The return echo is fed through a multichannel analyzer and an online computer system computes the PDF. From the PDF, it is determined how much of the 1.27 cm² contact area is dry and the change of dry wall fraction at increasing power levels is subsequently evaluated.

**Effect on CCP**

The effect, of a redefinition of Critical Heat Flux from OID to ODS, on CCP is illustrated on Figure 11: the shaded area represents the intermittent dryout ("Transition Boiling") region. Experiments on a full-length, water-cooled 37-element bundle showed that the ODS power (at which the first surface temperature in excess of 375°C was detected) was approximately 5% above the OID power (for the same channel flow).
FINAL REMARKS

The assessment study shows that the most promising CHF enhancement technique, from a CCP increase point-of-view, is the one using two additional planes of spacers and bearing pads.

An increase in CCP may also be obtained by using a more subdivided bundle, i.e. a bundle having smaller diameter elements and having more fuel elements per bundle. Such a bundle will have the added advantage of having a lower maximum linear power rating. A development program, to design and test a more subdivided bundle design, is currently in progress at CRNL.

ACKNOWLEDGMENTS

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REFERENCES


APPENDIX I: JUSTIFICATION FOR EXTRAPOLATION OF 37-ROD BUNDLE CHF AND PRESSURE DROP DATA

Strictly speaking, the CHF and pressure drop results obtained in a given fluid on a specific bundle geometry having a specific axial and radial flux distribution are only valid for that fluid, geometry and that particular axial and radial flux distribution.

Accurate modelling of CHF can only be achieved if the following similarity relations are satisfied:
- Identical geometry: i.e. the same heated length, cross sectional geometry, fuel appendages in both modf. and prototype
- Thermodynamic similarity: the same quality distribution, both radially and axially; this requires the same radial and axial flux distribution as well as the same boiling number, B°=CHF/h*G, where h*G is the latent heat and G is the mass flux.
- Property similarity: This requires that the liquid to vapour density ratio is similar in both fluids. It effectively means that the reduced pressure and the void fraction is the same in both fluids and that the important fluid properties have the same relative change with reduced pressure
- Hydrodynamic similarity: Basically, the mass flux in the prototype fluid (water) is related to the mass flux in the modelling fluid (Freon-12) using a mass flux scaling factor F, which usually is derived from experimental data.

Accurate fluid modelling of CHF in simple geometries has been established since 1964. CHF modelling in 37-element bundles had been proposed for many years but could not be validated until full length 37-element water CHF results became available. Since then, internal CRNL studies have demonstrated acceptable Freon-to-Water CHF modelling by predicting the horizontal water-cooled segmented-bundle results using the vertical Freon 37-element data. For flows above the stratification threshold, i.e. greater than 7.5 kg/s in water, Ahmad, Nickerson, and Midvidy (1982) subsequently confirmed the validity of Freon/Water CHF modelling and concluded that "CHF correlations obtained solely from Freon horizontal Freon data are capable of satisfactorily predicting CHF for 37-element water-cooled horizontal channels".

In summary, Freon-12 is an excellent test fluid for providing an estimate of the CCP for water-cooled bundles. However, definitive test of a full length water-cooled bundle is still recommended to provide a more exact estimate of the CCP value, to satisfy both design and licensing requirements.
THE ONSET OF SUBCOOLED NUCLEATE BOILING IN NUCLEAR FUEL BUNDLES

by

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Abstract

One of the requirements for the thermalhydraulic analysis of water cooled nuclear reactors is the prediction of two-phase pressure drop of the coolant in the fuel channel. Many existing pressure drop prediction methods employ two-phase multiplier correlations based on adiabatic gas-liquid flow. The paper shows that the use of such multiplier correlations with the thermodynamic equilibrium quality results in unsatisfactory predictions of the pressure profile in heated bundles. In order to compute a realistic vapour weight quality, a reliable Onset of Nucleate Boiling correlation valid for bundles must be used.

Existing correlations for the Onset of Nucleate Boiling (ONB) or the Onset of Significant Void (OSV) do not adequately predict the location in a reactor channel where boiling starts to affect the pressure profile. A reactor channel contains many subchannels of varying enthalpy and consequently boiling may occur in one subchannel and not in another at the same cross-sectional average conditions. Therefore, a new correlation was derived from some recently obtained 37-element CANDU*-geometry simulated bundle pressure drop data.

The emphasis of this paper is on the derivation of this Onset of Nucleate Boiling correlation. The paper also highlights the pressure profile predictions that may be obtained using the proposed ONB correlation.

1. INTRODUCTION

Knowledge of the pressure drop along a string of fuel bundles in a reactor channel is important in the design of an efficient nuclear reactor. The design inlet pressure must be sufficiently high to compensate for the channel pressure drop. When the coolant changes from single-phase to two-phase flow in the subcooled boiling region, the pressure gradient in the channel begins to steepen. The point where the rate of pressure drop changes is called the point of Onset of Nucleate Boiling (ONB). Since this point is the start of established two-phase flow, an accurate location of the ONB point is the first step to predict the two-phase pressure profile.

Most of the successful ONB correlations (1,2,3) are difficult to apply in reactor conditions because a knowledge of both saturation and wall temperature are required.

*CANADU - Canadian Deuterium Uranium, is a registered trademark of Atomic Energy of Canada Limited

The point where wall void is carried into the main stream without immediate collapse is called the Onset of Significant Void (OSV).

A very popular OSV correlation was derived by Saha and Zuber (4). Their correlation is valid for water and refrigerants and covers a fairly wide range of conditions. It can be simply expressed as

\[ x_{OSV} = \frac{1}{Bo} \]

where \( x \) is thermodynamic equilibrium quality at the OSV point and \( Bo \) is the Boiling Number, the ratio of the heat flux to the product of mass flux and latent heat.

Equation (1) was correlated to predict the local quality at the OSV point for tubes with a uniform heat flux. Equation (1) predicted the single-phase length in our experimental bundle data with an average error of +46.3% and an RMS error of 51.4%.

The error was defined as:

\[ \text{Error} = \frac{Z_{pred} - Z_{exp}}{Z_{exp}} \]

where \( Z \) is the heated channel length up to the ONB point.

Saha and Zuber's correlation therefore, predicted the dividing point between single and two-phase flow in a 37-element bundle at a much lower subcooling. This can be explained by the fact that multi-element channel flow is more complex than single-tube flow. At the same cross-section average heat flux condition, vapour will sooner be generated in the higher heat flux subchannels than in a tube with the same equivalent diameter, thereby affecting the two-phase pressure drop profile.

It is also evident from ASSEER studies (5) that there is little subcooled boiling in the subchannels. The observed subcooled two-phase pressure drop in the effect of enthalpy maldistribution in the subchannels.

Snook and Ahmad (6) previously derived an ONB correlation based on pressure drop data obtained from an experiment with a 37-element bundle with a uniform axial heat flux profile. Applying this correlation to non-uniform axial heat flux bundle data did not result in optimum results. The axial heat flux in a reactor channel is not uniform and may approximate a skewed cosine function (7). It is therefore important that
an ONB correlation can yield accurate predictions in a non-uniform axial heat flux field.

2. DATA BASE

The pressure drop data used for the correlation came from an experiment with a simulated CANDU-geometry bundle having a skewed cosine axial heat flux profile and using water as the coolant. Geometrically, the bundle was similar to a string of 12 aligned bundles with simulated end plates. The heat flux profile is shown in Figure 1.

Eleven pressure taps were installed along the test section. In order to obtain accurate pressure profile measurements in the two-phase region, the distance between pressure taps decreased towards the test section outlet.

Using as many different experimental conditions as was practical, the measured two-phase pressure profile was plotted along with the prediction of the single-phase pressure gradient. An example is shown in Figure 2. Vertical bars are shown at the visually determined ONB point and there where the thermodynamic equilibrium quality equals zero. From the example, it is obvious that the two-phase pressure profile starts well upstream from the location of zero quality.

In all, the ONB point was determined for 136 different experimental conditions in these ranges:

\[
4.6 < \text{Flow} < 16.4 \text{ (kg.s}^{-1}\text{)}
\]

\[
188^\circ\text{C} < \text{Inlet Temperature} < 293^\circ\text{C}
\]

\[
9.5 < \text{Inlet Pressure} < 11.5 \text{ (MPa)}
\]

\[
-0.03 < \text{Outlet Quality} < 0.41 (-)
\]

3. DEVELOPMENT OF THE CORRELATION

Using an expression of the form proposed by Saha and Zuber (4), the conditions at the ONB point were correlated with the local cross-section average quality. The resulting correlation is given by:

\[
X_{ONB} = -568 \text{Bo} \quad \ldots(2)
\]

where \(X_{ONB}\) is thermodynamic quality at the Onset of Nucleate Boiling,

\[
\text{Bo} = \text{Boiling Number} \left( \frac{\phi}{G} \right), \quad \text{and}
\]

\[
G = \text{Mass flux}
\]

\[
\phi = \text{Cross section average heat flux}
\]

\[
\lambda = \text{Latent heat of vapourization}
\]

This correlation predicted the data reasonably well. However, it was found that the prediction error was somewhat dependent on the inlet quality. At low subcooling, the correlation lost the edge of its predictive capabilities. For this reason, the data was re-optimized using an inlet quality term:

\[
X_{ONB} = -2150 \left( 1 - e^{-\frac{\text{In}}{5}} \right) \left( 1 + e^{-\frac{\text{In}}{5}} \right) \text{Bo}^{1.15} \quad \ldots(3)
\]

This inlet quality term gives the correlation correct asymptotic trends. When the inlet quality approaches zero, the quality \(X\) at ONB also converges to zero. When the inlet temperature is very far below saturation (large negative \(\text{In}\)), the inlet quality term effectively disappears.

When the inlet temperature is near saturation, the inlet quality term reduces the absolute value of \(X_{ONB}\).

For constant inlet conditions, an increase in heat flux causes the ONB point prediction to move upstream. An increase in pressure has a similar effect while an increase in mass flux will result in predictions closer to the point where the thermodynamic quality equals zero.

4. DISCUSSION OF RESULTS

When Equation (3) was applied to its own data base, the average and RMS single-phase length prediction errors were 0.6% and 13.2%, respectively. Figures 3, 4, 5 and 6 show that error is independent of the inlet quality, inlet pressure, mass flux and power input (or heat flux).

It must be noted that the larger errors usually corresponded to predictions of short single-phase lengths. This caused a relative small absolute error in length to be translated into a larger relative error.

The predictions of Equation (3) were also compared with data from other 37-element CANDU geometry pressure drop experiments.

The experimental ONB points were visually established from the pressure gradient indicated by the pressure measurements.

Fourteen representative scans were used from a pressure drop data base obtained with a 6 meter long bundle with a uniform axial heat flux. Figures 7 and 8 show that the single-phase length is predicted within the expected accuracy. These figures also indicate that the prediction error is not a function of the inlet quality and heat flux.

Subsequent to its experimental program, the bundle with the uniform axial heat flux profile was modified by replacing the upstream 3 meter heated length with an unheated section of the same geometry. This bundle was tested using much higher heat fluxes.

In this case, the single-phase pressure gradient extended past the unheated length into the heated section. Predictions of Equation (3) were compared with several of the pressure profiles of the pressure drop data base. The prediction errors are shown in Figures 9 and 10.

The prediction errors do not exhibit trends with the important parameters. The average error for these comparisons was about -7%.

The slight underprediction of the single-phase length in bundles with uniform axial heat flux can be explained as follows:

Assume a bundle with a uniform axial heat flux with a given set of inlet conditions. The ONB point occurs at point 2 along the channel. The thermodynamic quality at point 2 is equal to \(X\). Now assume a bundle with a cosine axial heat flux profile and the same inlet conditions as the bundle with the uniform axial heat flux profile. The power to the cosine bundle is adjusted such that at the same point 2 the thermodynamic quality also equals \(X\).

In both situations, an equal amount of power has been
added to the system between inlet and point Z, but at point Z the local cosine bundle heat flux will be much higher than the uniform heat flux. As subcooled void increases with heat flux, we may expect the cosine ONB point to be upstream.

In a cosine axial heat flux profile, the local heat flux decreases going upstream (the vast majority of ONB points occurred before the peak in the heat flux profile). The quality decreases also, but in the sense that it becomes more negative. The absolute value of constant A in the cosine ONB correlation will therefore be larger. The development of the thermal boundary layer in an increasing axial heat flux field is slower than in a uniform axial heat flux, but this effect is not significant enough to offset the above reasoning. Consequently, an ONB correlation based on cosine data will underpredict the ONB quality in uniform axial heat flux experiments.

Refrigerant-12 pressure drop experiments were performed with a bundle of similar physical configuration. The axial heat flux profile was uniform. There were only seven pressure taps installed along the channel. Because of the fewer pressure measurements along the channel, the visually established location of the ONB point was less accurate. Twenty out of 203 experiments were used to compare with the predictions of Equation (3). The average and RMS prediction errors were found to be -13.12 and 15.82. From the error distribution diagrams (Figures 11, 12, and 13), it can be seen that the quality at the location of OSV point was almost always underpredicted.

The error range appears to increase with an increase in mass flux as shown in Figure 13.

Equation (3) underpredicts the single-phase length in Refrigerant-12 by a factor of about 2 compared to water. As the latent heat of Refrigerant-12 is much smaller than that of water, the Boiling Number in Equation (3) becomes larger. Consequently, the predicted XONB decreases. For liquids other than water, Equation (2) is recommended for predicting the single-phase length in a heated bundle.

To find the ONB location in a reactor channel with non-uniform axial heat flux, for any given local and inlet conditions, a simple iterative solution scheme may be used:

Starting at the beginning of the heated length, compare the local thermodynamic quality and the predicted XONB from Equation (2 or 3). At the beginning of the heated length, the local quality is normally less than XONB. The comparison is repeated at intervals going downstream until the local quality and XONB converge.

Where XONB = X two-phase boiling regime starts as far as pressure drop calculation is concerned.

5. EXAMPLES OF PRESSURE PROFILE PREDICTION

Equations (2 and 3) were developed to facilitate the prediction of the pressure profile in a heated reactor channel. How well the proposed ONB correlation functions in a reactor channel pressure model is shown in the following figures.

Figure 14 shows the predicted pressure profiles superimposed on the measured pressure profile in a cosine axial heat flux bundle.

This figure shows clearly that any two-phase pressure drop model for use with complex heated geometries must include a method for predicting the ONB point. Figure 15 shows another example with a different heat flux and different inlet conditions.

Figures 16 and 17 show pressure profile prediction examples from the bundle with uniform heat flux and the bundle with a 3 meter unheated upstream section. For both cases, the ONB prediction is instrumental in predicting the correct pressure profile.

6. CONCLUSIONS

When predicting the pressure drop in CANDU fuel channels, it is essential that the two-phase flow pressure drop be evaluated starting from the point of ONB. Two correlations are recommended in this paper. Equation (2) is accurate and allows fast computing. Equation (3) is recommended when the inlet quality is high. The application of both equations in combination with suitable vapour weight quality and two-phase multiplier correlations resulted in very successful multi-element bundle pressure drop predictions.

7. ACKNOWLEDGEMENTS

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References


FIGURE 5 - EFFECT OF MASS FLUX ON PREDICTION ERROR (COSINE BUNDLE)

FIGURE 6 - EFFECT OF POWER ON PREDICTION ERROR (COSINE BUNDLE)

FIGURE 7 - EFFECT OF INLET QUALITY ON PREDICTION ERROR (6-m UNIFORM HEAT FLUX BUNDLE)

FIGURE 8 - EFFECT OF POWER ON PREDICTION ERROR (6-m UNIFORM HEAT FLUX BUNDLE)
FIGURE 9 - EFFECT OF INLET QUALITY ON PREDICTION ERROR (3-m UNIFORM HEAT FLUX BUNDLE)

FIGURE 10 - EFFECT OF POWER ON PREDICTION ERROR (3-m UNIFORM HEAT FLUX BUNDLE)

FIGURE 11 - EFFECT OF INLET QUALITY ON PREDICTION ERROR (6-m REFRIGERANT-12 BUNDLE)

FIGURE 12 - EFFECT OF POWER ON PREDICTION ERROR (6-m REFRIGERANT-12 BUNDLE)
FIGURE 13 - EFFECT OF MASS FLUX ON PREDICTION ERROR (6-m REFRIGERANT-12 BUNDLE)

FIGURE 14: Predicted and Measured Pressure Profiles (Cosine Bundle)

FIGURE 15: Predicted and Measured Pressure Profiles (Cosine Bundle)

FIGURE 16: Predicted and Measured Pressure Profiles (6-m Uniform Heat Flux Bundle)
FIGURE 17: Predicted and Measured Pressure Profiles (3-m Uniform Heat Flux Bundle)
CIRCUMFERENTIAL DRY PATCH SPREADING ON A SIMULATED CANDU* FUEL STRING WITH NON-UNIFORM AXIAL HEAT FLUX

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Abstract

In the past decade, a series of experiments was carried out at the Chalk River Nuclear Laboratories of Atomic Energy of Canada to obtain detailed thermal hydraulic information on CANDU fuel channels. The experiments were performed with simulated 37-element fuel strings of CANDU geometry.

CANDU bundles consist of 37 elements arranged in three rings around the central pin. Each element is 50 cm long and is kept in place between two endplates. Spacers at midplane prevent individual elements from bowing into contact with another element or the pressure tube. During the experimental program several strings simulating 12 CANDU bundles placed end to end were subjected to a detailed experimental investigation using water as the test-fluid. All strings were electrically-heated and simulated the radial heat flux depression observed in natural UO2 fuel.

The last string to be tested was built with elements of continuously varying wall thickness. The elements were assembled such that the axial flux distribution had the shape of a cosine, skewed towards the outlet end. Every element in the string was equipped with sophisticated measurement devices. One-half of the elements were equipped with Resistance Temperature Detectors for CHF-detection. The other half was instrumented with thermocouples. These thermocouples could be positioned at any axial and radial position inside the element by a remotely controlled thermocouple drive system.

The emphasis of this paper is on the effectiveness of the coolant to keep the fuel from overheating at heat fluxes beyond critical.

Detailed temperature maps were constructed for all elements that indicated high surface temperatures and dry patch spreading. Figures are shown with temperature maps for different conditions of flow, inlet subcooling and heat flux. Dry patch spreading with increasing heat flux is evident from these figures.

Even at very high fluxes, the temperature of a significant portion of the surface remained near saturation. The temperature of other areas indicated that the efficient nucleate boiling heat transfer had somewhat deteriorated. Only a fraction of the surface reached the temperature levels expected in a film boiling mode of heat transfer.

1. INTRODUCTION

Recently, there has been a renewed interest in greater reactor operating flexibility and efficiency. Increasing the neutron overpower trip set-points allows greater efficiency and flexibility of operation but reduces the margin to dryout.

Although the real limit to safe reactor operation is pressure tube integrity, the reactor power is conservatively restricted to power levels well below the Onset of Intermittent Dryout (OID) (1).

Operating beyond the dryout power or Critical Heat Flux (CHF) results in local "dry" spots with impeded heat transfer from fuel element to coolant. The size and shape of these areas determine if fuel failure or centrel ine melting will take place. In general, a small dry patch is not damaging. However, a large dry patch which is completely wrapped around the circumference of an element may cause centrel ine melting or fuel sheath failure due to overheating.

The extent of element sheath overheating and dry patch spreading was experimentally investigated using an electrically heated 37-element bundle (2). This bundle simulated a string of 12 aligned CANDU fuel bundles placed end to end. The bundle was six metres long and had simulated end-plates every 50 cm. Upstream and downstream unheated lengths of the same geometry acted to distribute the flow properly.

A realistic radial heat flux depression as well as a skewed cosine axial heat flux profile were part of the bundle design. Individual bundles in the string were indicated with the letters A-L as indicated in Figure 1.

Provisions were made to radially and axially scan the interior of several of the element sheaths with thermocouples. From these interior temperature measurements, the outside sheath temperature could be calculated, taking into account the heat flux, thermal conductivity and wall thickness. The elements equipped with thermocouples are indicated in Figure 2. Some thermocouple carriers were equipped with one thermocouple, others with two thermocouples placed 180° apart. The remaining elements were equipped with Resistance Temperature Detectors (RTD).

The experiments with the "cosine" bundle had three distinct goals, i.e., the measurement of critical power, pressure drop and the magnitude of the post-dryout temperatures.

This paper deals with the circumferential spread of
The Circumferential Drypatch Fraction was defined as the ratio of that part of the circumference where dry patches after the critical heat flux had been exceeded.

For given experimental conditions, the Critical Channel Power (CCP) was measured first. After the CCP was established, the electrical power to the bundle was increased in discrete steps. After every increment, the element sheath temperatures were scanned. In this way, an element temperature profile was obtained at various Critical Overpower Ratios (COR).

2. DATA BASE

The data was obtained from 12 experiments with different operating conditions and 6 repeat experiments. The flow ranged between (nominal) 9 and 16 kg.s⁻¹ and the inlet temperature varied between 187°C and 284°C. The pressure was approximately 10 MPa. During these experiments, the thermocouples were rotated and moved axially to obtain element temperature profiles.

The data reported in this paper was extracted from all experiments in which thermocouple scanning took place. In effect, any element that indicated an area with temperatures above 375°C was included.

The division of temperatures between values above and below 375°C was for convenience. An alternate choice of division would be to consider dryout to occur at a certain temperature difference above the saturation temperature. However, the exact magnitude of this temperature difference is difficult to establish. Also, the pressure changes along the test section and therefore the saturation temperature must be predicted at the location of each temperature measurement.

Element cooling just beyond dryout is still very effective in this 37-element geometry, a division at 375°C was deemed to be reasonable.

For each experiment at each power level, the temperature data for each individual element indicating temperatures beyond 375°C was plotted as shown in Figure 3. This figure shows the experiment number, date, experimental conditions and a 37-element bundle diagram for easy reference.

The temperature measurements are shown on their actual measurement location with different symbols for temperatures below and above 375°C. The element is shown folded open. A different map is shown for each power level.

The number of scans is indicated above each map. Where a bundle is equipped with 2 thermocouples, this results in a number of measurements which is double the number of scans.

The initial drypatch in the experiment shown in Figure 3 is located near the mid-length spacer plane. As the power to the bundle is increased, the initial drypatch grows and a second drypatch appears at the downstream end of the element. As the power is further increased, the drypatches in this particular experiment continue to grow and at the highest power measured, the downstream drypatch has wrapped around the entire circumference of the element. In general, it was observed that the extent of circumferential drypatch spreading was usually largest just upstream of the spacer plane and endplate.

The Circumferential Drypatch Fraction was defined as the ratio of that part of the circumference where dry inlet conditions were present to the entire element circumference.

The data for this study was taken from 99 different elements and experimental conditions.

The original maps measured 5 x 30 cm. This departure from true scale made it easier to measure the Circumferential Drypatch Fraction (CDF). At each location where a CDF was measured, the pertinent local conditions (pressure, heat flux, quality and coolant mass flux) were recorded.

Another example for different local thermalhydraulics conditions is shown in Figure 4. As with the previous example, it can be observed that the drypatch grows circumferentially as well as axially as the power is increased.

3. DRYPATCH SPREADING RESULTS

3.1 Repeatability

The data reported in this paper was extracted from all experiments in which thermocouple scanning took place. In effect, any element that indicated an area with temperatures above 375°C was included.

The division of temperatures between values above and below 375°C was for convenience. An alternate choice of division would be to consider dryout to occur at a certain temperature difference above the saturation temperature. However, the exact magnitude of this temperature difference is difficult to establish. Also, the pressure changes along the test section and therefore the saturation temperature must be predicted at the location of each temperature measurement.

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The Circumferential Drypatch Fraction was defined as the ratio of that part of the circumference where dry patches after the critical heat flux had been exceeded.

For given experimental conditions, the Critical Channel Power (CCP) was measured first. After the CCP was established, the electrical power to the bundle was increased in discrete steps. After every increment, the element sheath temperatures were scanned. In this way, an element temperature profile was obtained at various Critical Overpower Ratios (COR).

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Another example for different local thermalhydraulics conditions is shown in Figure 4. As with the previous example, it can be observed that the drypatch grows circumferentially as well as axially as the power is increased.

3.1 Repeatability

Repeatability of the measurements of shape, size and location of the drypatches gives an indication of the reliability of the data. In these experiments, exact duplication of experimental conditions was not feasible. In repeat experiments was usually encountered at a channel power a few percent above or below the original value. The cooling of the sheath surface in one subchannel is also very dependent on the thermalhydraulic conditions in neighbouring subchannels. In the highly turbulent and fickle environment of a 37-element bundle, we may expect to observe similarities between repeat tests. Exact duplication is difficult to achieve.

Repeat tests were scrutinized for location and size of the original drypatch on any element and its subsequent growth.

In general, the figures showed that the CDF data was fairly consistent. Good repeatability was observed in most elements between repeat experiments. In some cases, less than ideal comparison was noted. The consistency of the data was also indicated in the preferential dryout angle study described in the next section.

3.2 Preferential Dryout Angle

From the repeatability point of view, it is important to know where the drypatch originated in each experiment. Six elements that indicated dryout during most experiments were chosen for further study. They were elements J8, J24, J30 and elements K8, K24 and K30. In every experiment in which any of these elements indicated dryout, the “dry” circumferential angle was noted at its first occurrence. This was done for the area near the spacer plane, since the majority of initial dryouts occurred there. For example, element J8 in Figure 3 indicates a drypatch between the (estimated) angles of 260° and 310°. The circumferential angle was thus noted for each experiment in which element J8 experienced dryout. The number of dryout occurrences was plotted for each angle resulting in the graph shown in Figure 5.

This figure indicates that the preferential dryout angle of element J8 is near 270°, facing the subchannel between element 7 and the cold wall. Examples of the preferential dryout angles of some other elements are shown in Figures 6 and 7.
FIGURE 1 - SKEWED COSINE AXIAL HEAT FLUX PROFILE

FIGURE 2 - CROSS SECTION OF BUNDLE AS SEEN LOOKING DOWNSTREAM (WITH THE FLOW)

FIGURE 3: EXTENT OF DRYPATCH SPREADING AT FOUR LEVELS OF OVERPOWER
FIGURE 4: EXAMPLE OF DRYPATCH SPREADING

FIGURE 5 - DRYOUT LOCATION FREQUENCY OF BUNDLE J, ELEMENT 8

FIGURE 6 - DRYOUT LOCATION FREQUENCY OF BUNDLE K, ELEMENT 30
From these figures, it appears that the preferential dryout angle of element J30 is near 45°. The preferential dryout angle of element K30 is very clearly 180°, facing element 18. The difference in preferential dryout locations between these adjacent elements is difficult to explain.

3.3 Correlation of the Data

It is to be expected that parameters such as cross-section average local heat flux, mass flux and quality influence the spreading of a drypatch. An increase in the local heat flux will help a drypatch to grow, while an increase in mass flux or quality (an increase in quality increases the liquid velocity) will retard the growth of a drypatch due to enhanced cooling of the sheath.

For each Circumferential Drypatch Fraction (CDF) measured, the local conditions of heat flux (radially adjusted), flow, pressure and cross-sectional average quality were determined. The Critical Overpower Ratio (COR) was determined from the ratio of channel power and critical power at the first onset of dryout. The intent of this study was to develop a method to predict the Circumferential Drypatch Fraction in a given thermohydraulic situation.

For the correlation, 158 CDF data were available. Several expressions to correlate the independent variables with the CDF were explored, but a good correlation was not found. One cause of the poor correlation is the definition of the COR. At a COR of 1.1, an initial drypatch may have grown considerably, while another may just be at the point of being created, even in the same element ring and axial plane. That is, the nature of PDO heat transfer in a bundle: dryout in one subchannel increases the cooling in neighbouring subchannels by diversion of flow. Therefore, under similar conditions two elements (in the same ring) may indicate completely different Circumferential Drypatch Fractions.

3.4 Presentation of the Data

As the designer's interest in the CDF is not so much an average value but the prediction of an upper limit of Circumferential Drypatch Spreading, the CDF's were plotted versus the Critical Overpower Ratios. At each experimental overpower, only the maximum CDF in each of the bundle's heat flux regimes (rings) was considered. That is, in each bundle the maximum CDF of the elements 1-18 was used for the analysis. The CDF's of other elements in that ring with lower values of CDF were discarded. The same procedure was applied to the other rings in the bundle. The drypatch areas near the spacer plane and endplate were considered separately.

The plot is shown in Figure 8. The different symbols shown correspond to different ranges of local average quality and Boiling Number (Bo), where the Boiling Number is defined as the ratio of heat flux to the product of mass flux and latent heat.

From Figure 8, it is clear that the data do not correlate well.

The solid line shown is the curve above which no CDF was observed and as such is the CDF upper bound. This curve may be described by the simple parabola

\[ \text{CDF} = 18.7 \cdot (\text{COR})^2 - 32.5 \cdot \text{COR} + 13.8 \]  

Equation (1) limits the magnitude of the Circumferential Drypatch Fraction for any given value of Critical Overpower Ratio. For example, at COR = 1.1, the maximum observed CDF was about 0.5. Equation (1) limits the CDF to approximately 0.7. The CDF prediction method indicates that complete circumferential dryout did not occur below COR = 1.13.

By separating the CDF values between endplate data and spacer plane data, it was observed that the maximum CDF below 0.5 was dominated by the spacer plane data. A higher overpower was needed to initiate drypatch near the downstream endplates. At higher overpowers, however, the maximum CDF occurred frequently near the endplates.

3.5 Prediction of Other CDF Data

In order to test the validity of Equation (1), its predictions were compared with CDF data from other (proprietary) sources. The CDF predictions compared well with this data, considering that the CDF from these other sources was based on the first indication of dryout and not on a given minimum temperature, well beyond the saturation temperature (375°C in this study).

3.6 Outer Ring Elements

The temperature of an element in partial or complete circumferential dryout will increase in proportion to the Circumferential Drypatch Fraction and drypatch size. Temperature gradients or severe overheating of an element may cause it to bow in the direction of the area with the higher temperatures. An element bowed into contact with the pressure tube will increase the tube's temperature. This temperature increase in turn decreases the tube's reliability as a pressure vessel.

To explore the possibility of element/pressure tube contact due to element bowing, the drypatch spreading of the outer ring elements (l=18) was examined separately.

The circumferential drypatch spreading at both preferential dryout locations (near spacer plane and near endplate) on the outer ring elements is shown in Figure 9.

The solid line shown in Figure 9 is the outer ring elements upper bound CDF and may be described by

\[ \text{CDF}_{OE} = 21.5 \cdot (\text{COR})^2 - 40.0 \cdot \text{COR} + 18.5 \]  

where CDF_{OE} equals the Circumferential Drypatch Fraction for the outer ring elements and COR equals the Critical Overpower Ratio.

It is clear from a comparison between Figures 8 and 9 that at any COR, the CDF for the outer elements is about 20-30% lower than for all elements combined.

For the outer ring elements, the overpower ratio needed to complete circumferential dryout has increased from 1.141 to 1.156.

The remainder of CDF information was derived from intermediate ring data. Therefore, the intermediate ring elements drypatch spreading determines the upper bound CDF.
4. SUMMARY

1. Experimental temperature data from 12 “Cosine Bundle” experiments with different operating conditions and 6 repeat experiments were used to examine the circumferential drypatch spreading on elements beyond the critical heat flux.

2. It was concluded that the consistency of the data is good and that the repeatability is acceptable.

3. The element cooling is better than previous methodologies would suggest.

4. The preferential dryout angle was determined for several elements. Especially elements J8, J24, J30, K8 and K30 indicated an affinity for a particular dryout angle.

5. Complete circumferential dryout was not observed until the critical bundle power had been exceeded by over 14%.

6. The upperbound Circumferential Drypatch Fraction may be described by a simple parabola in terms of the Critical Overpower Ratio.

7. The upperbound CDF relationship (Equation 1) adequately predicted additional CDF’s obtained from other sources.

8. The upperbound CDF for the outer ring elements only ranges between 20 and 30% below the intermediate ring upperbound CDF.

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References


FIGURE 9 - CIRCUMFERENTIAL DRYPATCH SPREADING (OUTER RING ELEMENTS ONLY)
ABSTRACT

The one-dimensional two-fluid momentum equations are used to predict the liquid surface profile in steady co-current stratified flow of gas and liquid in a horizontal tube and rod bundle tube. For given gas and liquid flow rates, the liquid surface profile is predicted in relation to three reference levels, namely the equilibrium and two critical levels.

The void fraction in a series of stratified flow tests is predicted using the fact that the separation tank at the test section outlet in the test facility imposes a free fall condition on the liquid phase. Good agreement between the predicted and observed void fractions is obtained.

For the same test conditions, the void fraction is also predicted in a CANDU reactor fuel channel type geometry where the channel ends in vertical pipe instead of a separation tank. For this geometry, the predicted channel void fraction is found to be significantly lower than that for a geometry with a separation tank.

INTRODUCTION

Stratified two-phase flow occurs at low flow rates in horizontal industrial pipelines. Examples are the flow of oil and natural gas in pipelines and steam and water in nuclear heat transport pipes following a postulated loss of coolant accident. To study flow pattern transition and heat transfer [1] in stratified two-phase flow, the liquid level (equivalently the void fraction) needs to be known.

In previous studies, the liquid level was either identified with the equilibrium level [2,3,4] or a correlation, such as Martinelli’s, was used [5,6,7]. The correlations are based largely on test data on immiscible fluids, such as oil and gas and air and water, and on test facilities with a separation tank at the downstream end of the test section.

In the present study, the one-dimensional two-fluid conservation equations are used to predict the liquid level in steady co-current stratified flow of gas and liquid with no heat addition in a horizontal tube and rod bundle tube. This flow pattern could occur in a CANDU reactor fuel channel under low flow conditions.

Section 2 of this paper presents the relevant two-fluid conservation equations. Section 3 defines and discusses the physical significance and properties of the equilibrium and critical levels. Section 4 discusses the behaviour of the liquid surface. In Section 5, implicit closed form solution for the void fraction for various flow regimes is given. Section 6 describes the WNRE RD-13 stratified flow test facility and presents the results of a series of tests. The measured and predicted void fractions in these tests are compared in Section 7. Section 7 also presents the predicted void fraction for a boundary condition appropriate for a reactor fuel channel geometry. Finally, Section 8 summarizes the major results.

GOVERNING CONSERVATION EQUATIONS

Mass and momentum balance on a fluid element in steady, adiabatic, incompressible gas-liquid stratified flow in a horizontal pipe give (refer to the table of nomenclature):

\[ \frac{j_k}{\gamma_k} = \alpha_k u_k = \text{constant}, \quad k = g, l \]  

\[ \frac{1}{\gamma_k} \left[ \alpha_k^2 - F_k^2 - \rho_k \rho_l \frac{2}{\gamma_l} \cdot \alpha_k \cdot \frac{1}{\alpha_l} \alpha_l \right] \frac{d}{dz} \alpha_k = 1 + F_k^2 \left( \frac{\alpha_k}{\alpha_l} \right)^2 \]  

\[ \frac{d}{dz} \alpha_k = \frac{\alpha_k}{\alpha_l} \]  

where

\[ \tau = \frac{f_k F_k^2}{2D}, \quad F_k = \frac{j_k}{j_c}, \quad j_c = \sqrt{\gamma_l h} \]  

\[ \rho_k = \rho_l, \quad F_k = \frac{f_k}{f_2} \left( \frac{\rho_k}{\rho_l} \right)^2 \]  

To obtain the gas-liquid combined momentum equation (2), the interfacial drag has been neglected as it is insignificant in this application. In eq. (2) circular pipe
cross-section has been replaced by a square one of equal area. This is a convenient and a reasonable approximation. The value of the constants K and n depends on the flow regime. In this paper, the following criterion [6,8,9] for flow regime transition is used:

\[
K = 64.0, \quad m = 1.0 \quad \text{for} \quad Re_k^S \leq 1600 \\
\quad (\text{laminar flow})
\]

\[
K = 0.184, \quad m = 0.2 \quad \text{for} \quad Re_k^S > 1600 \\
\quad (\text{turbulent flow})
\]

where \( Re_k^S \equiv j_k D/v_k \) is the superficial Reynolds number for phase \( k \).

In eq. (3), \( j_k \) is the full channel critical speed. Physically, the critical speed is the speed with which a small disturbance propagates on a fluid surface of depth \( H \).

It is convenient to express eq. (2) in the following form:

\[
\frac{1}{\gamma_g} X(a_g) \frac{d}{dz} a_g = Y(a_g) 
\]  

(5)

where

\[
X(a_g) = \alpha_g^3 - F_k^2 - \rho_g \frac{F_k}{\alpha_k} \alpha_k^3 \\
Y(a_g) = -1 + F_k^2 \frac{\alpha_k}{\alpha_g^2}
\]  

(6)

The gas and liquid momentum equations from which eq. (2) is derived, are readily expressed in terms of the phase mechanical energy \( E_k \) to obtain:

\[
\frac{d}{dz} E_k = -\frac{\gamma_k}{\gamma_k} \cdot \tau_k 
\]  

(7)

where

\[
E_k = \frac{1}{2} \rho_k u_k^2 + g \rho_k h + P_k, \quad \tau_k = \frac{1}{8} \rho_k u_k^2 \frac{\alpha_k}{\alpha_g^2}
\]  

(8)

For given gas and liquid flow rates, \( E_k \) is a function of \( a_g \) only. It then follows that:

\[
\frac{d}{dz} E_k = \frac{d}{dz} E_k \frac{d}{dz} a_g 
\]  

(9)

Eq. (7) shows that \( \frac{d}{dz} E_k \) is always negative and, hence, the mechanical energy of each phase is dissipated continuously in the direction of the flow.

**EQUILIBRIUM AND CRITICAL LEVEL**

Solution of eq. (5) is closely related to the roots of \( X \) and \( Y \). These roots are the equilibrium and critical levels.

**The Equilibrium Level**

Figure 1 shows the behaviour of the function \( Y \) with \( a_g \) for given gas and liquid flow rates. \( Y \) is a monotonically increasing function of \( a_g \) and vanish--at the equilibrium level \( a_{gE} \) given by:

\[
Y(a_g = a_{gE}) = 0 
\]  

(10)

It follows from eq. (2) that the equilibrium level is given by balance of gas and liquid frictional pressure drops (and interfacial drag). This level is identical to that used by Taitel-Dukler [2].

**The Critical Levels**

Depending on the gas and liquid flow rates, the function \( X \) in eq. (5) may vanish at a critical level \( a_{gC} \) given by:

\[
X(a_g = a_{gC}) = 0 
\]  

(11)

At a critical level, eq. (7) shows that the spatial gradient of \( a_g \) is infinite and, hence, the level profile is vertical. It is shown below that such large surface curvature could occur only at the ends of a pipe.

The momentum equation (5) is a generalization of that for water flow in a horizontal open channel [10] where the stationary atmosphere replaces the flowing gas. Thus, in a horizontal open channel flow, there is no equilibrium level and only one critical level given by:

\[
Fr \equiv \frac{u_k}{\sqrt{gh}} = 1 
\]  

(12)

where \( \sqrt{gh} \) is the critical speed and \( Fr \) is the Froude number. Thus, for a critical level in an open channel, the Froude number is unity. It can be shown that, in a critical flow, the mechanical energy is minimum [10].

Definition (11) of the critical level in gas-liquid flow in a horizontal pipe may be considered as a generalization of eq. (12). However, in a two-phase flow there are, in general, two critical levels. Figure 1 shows the behaviour of \( X \) and \( a_g \) for given gas and liquid flow rates. The function \( X \) has a maximum at \( a_o \) given by:

\[
a_o = 1 - (\rho_o F_k^2)^{\frac{1}{3}}
\]  

(13)
It follows from eq. (14) and Figure 1 that \( X \) is negative for all values of \( \alpha \) if the flow rates are such that condition:

\[
F_0^2 + \left( \rho \phi F_2^2 \right) < 1 \tag{14}
\]

is satisfied. If (16) is satisfied, critical levels do not exist.

The energy equation (7) shows that, in a (rough) pipe, the mechanical energy of either phase is continuously decreasing in the direction of the flow. Thus, a liquid surface profile, for which the energy of the gas and/or liquid phase is either maximum or minimum anywhere between the ends of the pipe, is not allowed. A liquid surface profile, for which the energy of either or both phases is maximum at the entrance and/or minimum at the exit (but not vice-versa) of the pipe is, of course allowed.

The functions \( X \) and \( Y \) and, hence, \( \frac{dE}{dz} \) in eq. (5) and \( \frac{dE}{dz} \) in eq. (9) change sign wherever \( \alpha \) crosses a root of \( X \) or \( Y \). It is, therefore, natural to study the behaviour of the liquid surface profiles in relation to the positions of the equilibrium (i.e., the root of \( Y \)) and critical (i.e., the roots of \( X \)) levels and their energy states. This is done below.

For (high) gas and liquid flow rates satisfying condition (14) in a horizontal pipe, critical levels do not exist. The equilibrium level, \( \alpha_{eq} \), may be imagined to divide the pipe cross-section into an upper and a lower region as indicated by the dotted lines \( \alpha_{eq} \) in Figure 2(a). For given flow rates satisfying condition (14), the function \( X \) in eq. (5) is negative for all values of \( \alpha \). Figure 1 shows that the function \( Y \) in eq. (5) is positive in the pipe region above and negative in the region below the (imaginary) equilibrium level (i.e., for \( \alpha > \alpha_{eq} \) and \( \alpha < \alpha_{eq} \), respectively). It then follows from eq. (5) that \( \frac{dE}{dz} \) is negative for \( \alpha > \alpha_{eq} \) and positive for \( \alpha < \alpha_{eq} \) as shown on the right hand side in Figure 2(a). At \( \alpha = \alpha_{eq} \), \( Y \) vanishes. Eq. (5) then shows that \( \frac{dE}{dz} \) approaches zero as \( \alpha_{eq} \) approaches \( \alpha_{eq} \).

The variation with \( \alpha \) of the mechanical energy \( E \) of each phase is now readily determined. Since the signs of \( \frac{dE}{dz} \) in the regions \( \alpha > \alpha_{eq} \) and \( \alpha < \alpha_{eq} \) is now known (Figure 2(a)) and \( \frac{dE}{dz} \) is always negative, eq. (9) then shows that \( \frac{dE}{dz} \) is positive for \( \alpha > \alpha_{eq} \) and negative for \( \alpha < \alpha_{eq} \) as indicated on the left hand side in Figure 2(a). At \( \alpha = \alpha_{eq} \), \( \frac{dE}{dz} \) vanishes. Thus, \( \frac{dE}{dz} \) becomes infinitely large as \( \alpha \) approaches \( \alpha_{eq} \). These results give the schematic plot of \( E \) versus \( \alpha \) shown on the left hand side in Figure 2(a). This plot shows that the energy of each phase is minimum whenever along the pipe the liquid surface is at the equilibrium level.

The admissible liquid surface profiles are now easily determined. In the pipe region above the equilibrium level, \( \frac{dE}{dz} \) is negative and, therefore, the liquid surface falls with \( z \) toward the equilibrium level as shown schematically by the solid curve on the right hand side in Figure 2(a). The equilibrium level is, however, approached asymptotically and, hence, is not reached in a pipe of finite length. The admissible liquid surface profile in the region \( \alpha > \alpha_{eq} \) is then shown schematically by the solid curve on the right hand side in Figure 2(a).

The admissible liquid surface profile in the pipe region below the equilibrium level is
Similarly determined and is shown schematically on the right hand side in Figure 2(b). Specification of the liquid level at a point along the pipe (i.e., a boundary condition) allows one to select one of the two liquid surface profiles in Figures 2.

For gas and liquid flow rates satisfying the inequality in condition (15), each of the three configurations of the equilibrium \( \sigma_{e_1} \) and critical \( \sigma_{c_1} \) and \( \sigma_{c_2} \) levels may be imagined to divide the pipe cross-section into four regions as indicated by the dotted lines in each of Figures 3, 4 and 5. (The case of the equality in (15) for which the critical levels overlap and the case where either one or both critical levels and the equilibrium level overlap are not considered in this paper but they can similarly be treated.)

Figures 3, 4 and 5 show respectively the configurations \( \sigma_{e_2} > \sigma_{e_1} > \sigma_{c_1} \), \( \sigma_{e_2} > \sigma_{c_1} > \sigma_{e_1} \) and \( \sigma_{c_2} > \sigma_{e_2} > \sigma_{c_1} \). For each of these level configurations, the signs of \( \frac{d\sigma}{dz} \) and \( \frac{dE_k}{d\sigma} \) are readily obtained in each of the four (imaginary) regions of the pipe from eq. (5) and the signs of \( X \) and \( Y \) in Figure 1. These signs are shown on the right and left hand sides in Figures 3, 4 and 5. At a critical level, \( X \) vanishes. Eq. (5) then shows that \( \frac{d\sigma}{dz} \) is infinite at a critical level.

These results then give the schematic plot of \( E_k \) versus \( \sigma_e \) shown by the solid curves on the left hand side in Figures 3, 4 and 5. These plots show that, for a given configuration of equilibrium and critical levels, the energy of the phases is maximum wherever the liquid surface is at one of these levels and the energy is minimum wherever the liquid surface is at any one of the other two levels.
In each of the four (imaginary) regions of the pipe in figures 3, 4 or 5, the admissible liquid surface profile is now readily deduced from the following observations. The liquid surface cannot reach the equilibrium level in a pipe of finite length as shown above. At a critical level, the liquid surface is vertical since $\frac{d\psi}{dz}$ is infinite.

The liquid surface can be at a critical level only at the pipe entrance where the energy must be maximum or at the pipe exit where the energy must be minimum (and where upward or downward pipe connections would allow a vertical liquid surface). One then deduces the schematic liquid surface profiles shown by the solid curves on the right hand in Figures 3, 4 and 5. Again specification of the liquid level at a point along the pipe (i.e., a boundary condition) allows one to select one of the four liquid surface profiles for any one of the three configurations of the equilibrium and critical levels given in Figures 3, 4 and 5.

**COMPUTATION OF LIQUID SURFACE PROFILE**

For each gas and liquid flow regime, the momentum equation (5) is readily integrated to obtain an implicit solution for the liquid level or void fraction as a function of the axial distance. This has been done for the most commonly encountered flow regimes, i.e., laminar-gas laminar-liquid and turbulent-gas laminar-liquid. The detailed solution is lengthy and is not given here.

The above solution does not determine the void fraction uniquely, i.e. at a given point along the pipe, two or four different values of the void fraction is allowed. A boundary condition, i.e., the knowledge of the void fraction at some point along the pipe, and Figures 2-5 are used to select an appropriate value of the void fraction.

**TEST FACILITY AND RESULTS**

Many experiments have been conducted to study stratified flow and transition to other flow patterns in tubes [3,8,11] and rod bundle tubes [6,12]. One such an experiment was done recently in the RD-13 test facility at WNRE (Whiteshell Nuclear Research Establishment). The RD-13 facility (Figure 6) consists of a transparent horizontal test section 3.67 m long and containing a string of six seven-element rod bundles. The test section is connected upstream to a supply source of air and water and downstream to a separation tank. Compressed air and pumped water are supplied to a mixer and passed through the test section and to the separation tank where water drops to the bottom and air rises to the top. Test section inlet fluid volumetric flow rates and temperature and test-section pressure were measured. Cross-section averaged void fraction was measured with an accuracy of ± 0.035 at 1.5 m and 2.0 m downstream of the test-section inlet using gamma ray densitometers.

The test procedure was to bring the loop to the desired operating pressure. With the air flow rate adjusted to a preselected value, the water flow rate was increased in steps. At each step, conditions were allowed to stabilize before recording the data.

Figure 7 shows the void (crosses) measured at 1.5 m downstream of the test-section inlet as a function of Martinelli parameter $\chi$. This parameter is the ratio of water to air (superficial) frictional pressure drops, i.e.:

$$\chi^2 = \left(\frac{d\psi}{dz}\right)_l^s \left(\frac{d\psi}{dz}\right)_g$$

$$= \frac{K_x}{\rho_g K_g} \left(\frac{v}{D}\right) \left(\frac{D}{D}\right)^m$$

The measured void fraction in Figure 7 increases with decreasing $\chi$, gradually for laminar-gas laminar-liquid flow regime, and relatively rapidly for turbulent-air laminar-water flow regime.

**PREDICTED VOID FRACTION AND COMPARISON WITH EXPERIMENT**

Generally, experiments, such as that described in the previous section, conducted to study stratified flow in a horizontal tube have used a separation tank at the downstream end of the test section. Thus, the water phase in these experiments is subject to a free fall condition in the separation tank at the test section outlet. Experiments on open channel flows with free overfall have shown that, for subcritical upstream flow, the liquid level decreases and the velocity increases as the drop-off is approached. At the brink, the liquid level is critical. If the upstream flow is critical or supercritical, the liquid velocity increases as the drop-off is approached. Depending on the length of the channel, the liquid level at the brink may be lower than the critical level or the liquid may undergo a hydraulic jump causing the level to rise before the drop-off and then drop to the critical level at the brink [13].

The brink depth for convergent streamlines is less than the critical depth in a parallel flow (i.e. a flow with parallel streamlines). The brink depth is, however, not yet known accurately in a pipe flow, particularly a two-phase pipe flow. The liquid surface profile in the RD-13 stratified flow...
PRESSURE : 0.255 MPa  
TEMPERATURE : 17 - 23°C

X : OBSERVED VOID  
• : PREDICTED VOID WITH FREE FALL AT TEST SECTION OUTLET  
△ PREDICTED VOID WITH VERTICAL TAKE-OFF AT TEST SECTION OUTLET  
○ : EQUILIBRIUM VOID

Tests described in section 6 is, therefore, predicted as follows. For some of the flow rates used, the lower critical level is predicted to be below the equilibrium level as in Figures 3. For these test conditions, the liquid surface at the test section outlet (i.e. at the brink of the free overfall) is assumed to be at the lower critical level (a<sub>LC</sub> in Figures 3). The predicted liquid surface profile would resemble the schematic profile shown in Figure 3(c). For the remaining flow rates used, the equilibrium level is predicted to be below the lower critical level as in Figures 4. For these test conditions, the liquid surface at the test section outlet is assumed to be at the lower critical level (a<sub>LC</sub> in Figure 4(c)).

Figure 7 shows the void fraction (dots) as a function of Martinelli parameter at 1.5 m downstream of the test section inlet as predicted by the solutions of eq. (5) subject to the above boundary conditions. The measured void fraction (crosses) are also shown for comparison. The agreement between the observed and predicted voids is good. Figure 7 shows that these void fractions, however, differ significantly from the equilibrium void (circles with dots) predicted by eq. (10) particularly for the laminar-gas laminar-liquid flow regime.

The piping arrangement in the RD-13 facility differs significantly from that in a CANDU reactor fuel channel assembly. A reactor fuel channel is effectively connected at each end to a vertical feeder pipe rather than a separation tank as in the RB-13 facility. Thus, the free fall condition in the RB-13 stratified flow tests is not applicable to a flow condition in a reactor fuel channel. Steam produced in a fuel channel flows to the downstream feeder and escapes with water through the vertical feeder. (It is assumed that the pressure difference between the two headers at the top ends of the feeders is high enough to oppose the gravity head induced by the steam in the outlet header and, hence, maintain the steady stratified flow of steam and water.) The frictional pressure drop in the steam phase induced by the steam flow causes the water surface to rise near the downstream end of the channel. Thus, for the same flow rates, the void fraction in a fuel channel is expected to be everywhere significantly smaller than that in the RD-13 facility.

To confirm this expectation, the void fraction in the RD-13 channel is predicted using the solutions of eq. (5) subject to the boundary condition that the liquid level at the end of the test outlet be at the upper critical level, i.e. at a<sub>UC</sub>. The liquid surface profile predicted with this boundary condition resembles the schematic profile shown in Figure 3(b) if the lower critical level lies below, and in Figure 4(b) if the lower critical level lies above, the equilibrium level.

Figure 7 shows the predicted void fraction (triangles with dots) at 1.5 m downstream of the test section inlet. The void fraction coincides.

FIGURE 7 OBSERVED AND PREDICTED VOID FRACTION IN AIR-WATER STRATIFIED FLOW IN RD-13 HORIZONTAL ROD BUNDLE AT 1.5 m DOWNSTREAM OF TEST SECTION INLET
with the predicted equilibrium void fraction. The void fraction is seen to be generally much lower than the void fraction predicted (dots) and measured (crosses) in the RD-13 facility with the separation tank at the test section outlet. Thus, for the same flow rates, the void fraction in a CANDU fuel channel (with vertical feeder pipe arrangement) is expected to be smaller than that measured in the RD-13 facility (with the separation tank arrangement).

In Figure 7, predicted void fractions (i.e. triangles with dots) corresponding to higher gas and lower liquid flow rates in the RD-13 tests are not shown. For these flow rates, it is predicted that the liquid surface rises steeply with z and reaches the top of the pipe in a short length of the pipe. Thus, for the flow conditions in these tests, stratified flow pattern cannot exist along the entire length of the pipe. The flow pattern, for these conditions, is more likely to be slug flow.

CONCLUDING REMARKS

The liquid surface profile in steady, co-current stratified flow in a horizontal pipe is predicted using the one-dimensional, two-fluid conservation equations with no heat addition. This prediction is made in relation to three distinct reference levels, namely the equilibrium and two critical levels. At these levels, the mechanical energy of the phases is extremum. The liquid surface approaches the equilibrium level asymptotically as it becomes horizontal. At the critical level, however, the liquid surface is vertical.

For given flow rates, the momentum equations are integrated to obtain the void fraction as an implicit closed function of the distance along the pipe and consistent with energy conservation.

The void fraction in the WNRE RD-13 facility is predicted by realizing that the separation tank at the test section outlet imposes a free fall condition on the liquid phase. The predicted and measured void fractions in a test section cross-section are in good agreement with one another and they are generally different from the equilibrium void fraction.

For the flow rates in the RD-13 tests, the void fraction is also predicted for a boundary condition appropriate for a test section connected at the outlet to a vertical pipe as is the case in a CANDU fuel channel. The predicted void fraction is significantly smaller than that for the test section with a separation tank at its outlet.

For the vertical pipe arrangement, it is also found that stratified flow pattern cannot exit in the test section for the RD-13 tests at higher gas and lower liquid flow rates.

It is concluded, therefore, that in a stratified flow in a horizontal pipe, downstream boundary condition influences not only the upstream void fraction but also the range of flow rates over which stratified flow pattern can be maintained.

In this range of flow rates, however, downstream boundary condition may not affect stratification threshold flow rates.

Acknowledgement

The RD-13 tests were funded under the CANDU program.

References


Table of Nomenclature

D : Hydraulic diameter
E : Mechanical energy
\( f \) : D'Arcy friction factor
F : Ratio of superficial to critical speed, defined in eq. (3)
g : Gravitational acceleration
H : Square pipe width
j : Superficial velocity
K : Loss factor defined in eq. (4)
p : Pressure
Re : Reynolds number
u : Fluid velocity
X,Y : Functions defined in eq. (6)
z : Distance variable

Greek Symbols
\( \alpha \) : Fraction of pipe cross-section occupied by fluid
\( \nu \) : Kinematic viscosity
\( \rho \) : Density
\( \rho^g \) : Gas to liquid density ratio, defined in eq. (3)
X : Martinelli parameter, defined in eq. (16)
\( \tau \) : Shear stress

Subscripts
\( g \) : Gas
i : Interface
k : g or t
l : liquid
AIR-WATER FLOODING IN A 90° ELBOW WITH A SLIGHTLY INCLINED LOWER LEG

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ABSTRACT

Experimental results of the effect of slight inclinations of the lower leg of an elbow on its flooding limit in countercurrent air-water flow are presented. For a given water flow, an elbow with a slightly upwardly inclined lower leg requires much less air flow to cause flooding than the same elbow with a horizontal leg, whereas an elbow with a slightly downwardly inclined lower leg requires more. The implications of the results of the present work in the analysis of certain postulated upset conditions in a CANDU heat transport system are discussed.

NOMENCLATURE

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Definition</th>
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<tbody>
<tr>
<td>A</td>
<td>cross-sectional area of pipe</td>
</tr>
<tr>
<td>D</td>
<td>pipe diameter</td>
</tr>
<tr>
<td>g</td>
<td>gravitational constant</td>
</tr>
<tr>
<td>( j_g )</td>
<td>superficial gas velocity</td>
</tr>
<tr>
<td>( j_f )</td>
<td>superficial liquid velocity</td>
</tr>
<tr>
<td>( m_g )</td>
<td>mass flow rate of gas phase</td>
</tr>
<tr>
<td>( m_f )</td>
<td>mass flow rate of liquid phase</td>
</tr>
<tr>
<td>( \rho_g )</td>
<td>gas phase density</td>
</tr>
<tr>
<td>( \rho_f )</td>
<td>liquid phase density</td>
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INTRODUCTION

The core of a CANDU-PWR reactor consists of a number of horizontal fuel channels connected to headers above the core by individual feeder pipes which consist of horizontal, vertical and inclined sections. In the event of a loss-of-coolant accident (LOCA), an emergency coolant system is available which is designed to inject water into the headers for cooling the core. In certain postulated large break LOCAs, rapid channel voiding is predicted followed by a prolonged period of near-zero header-to-header pressure drop in the breached circuit. The delivery of emergency cooling water to the core is expected to take place very slowly under these conditions. Experiments conducted in a full-scale representation of a CANDU channel-feeder system have shown that flow of the injected water from the headers to the channel under these conditions is limited by countercurrent flow effects (1). Steam, generated when water contacts the hot feeders, flows countercurrent to the injected water tending to limit its flow. The flow of water is limited especially in 90° elbow sections of the feeder piping. This phenomenon of countercurrent flooding has been well studied in the literature. It occurs in many gas-liquid countercurrent-flow situations, when the gas flow cannot be increased beyond a certain limit without limiting the liquid flow or vice versa. The limit is termed the flooding point.

Flooding in an Elbow

Flooding in straight pipes has been extensively studied. However, relatively little information is available on flooding limits in an elbow geometry. Richter et al. (2) and Krowlewski (3) presented countercurrent flooding limits in air-water flow in a horizontal pipe connected at the inlet to either 45° or 90° elbows. Gardner (4) measured the flooding limit of air-water flow in a horizontal pipe connected at the inlet to a vertical water tank. The experiments showed that the gas-flow rate required to produce flooding in an elbow geometry is much smaller than that for a vertical pipe.

Recently, Siddiqui et al. (5) measured the flooding limit in air-water flow in a 90° elbow. Flooding was found to be due to unstable wave formation (slugging) at the hydraulic jump, which formed in the horizontal leg of the elbow close to the bend (see Figure 1). The flooding limit depended on the pipe diameter, the length of the lower leg, and the radius of curvature of the bend. Additionally, they observed that the flooding limit was sensitive to small changes (<1°) in the inclination angle of the lower leg of the elbow.

Figure 1. Typical flow pattern in an elbow before onset of flooding
Ardron and Banerjee (6) presented an analysis of flooding in an elbow where there was no interphase mass transfer. The model assumed a smooth stratified flow in the lower leg of the elbow. A correlation relating the nondimensional gas velocity and the void fraction at the hydraulic jump location was used and a free outfall condition was assumed at the exit of the lower leg. Model predictions of the flooding limit as a function of the length-to-diameter ratio and the angle of inclination of the lower leg of the elbow were in reasonable agreement with air-water experimental results.

Wan (7) measured the flooding limit in steam and subcooled (6°C) water flow in a 90° elbow. At low water flow rates, the flooding limit for steam-water flow agreed with air-water elbow flooding data. This implies that flooding, at low water flow rates, is hydraulics limited; at higher water flow rates, flooding behaviour is determined by condensation effects of steam in subcooled water.

Flooding in an Elbow with a Slightly Inclined Lower Leg

As previously mentioned, a slight inclination of the lower leg of an elbow has been observed to change the flooding limit significantly. This is an important finding particularly with regard to feeder refilling calculations in CANDU LOCA analysis for existing channel feeder geometries and for proposed future designs. It was therefore decided to investigate thoroughly the effect of small inclinations of the lower leg of an elbow on its flooding behaviour in air-water flow. The present work describes the experiments conducted and the results obtained.

EXPERIMENTAL SET-UP AND PROCEDURE

Figure 2 shows a schematic of the test rig. The test facility consisted of an upright (~90°) glass elbow with a supply tank and a separation tank connected to its horizontal and vertical legs, respectively.

Distilled water was circulated in a closed loop by a pump. Water, drawn from the bottom of the supply tank, flowed through a heat exchanger before entering the pump. An automatic flow controller regulated the flow of cooling water to the secondary side of the loop to maintain the loop water temperature constant. The water flowed through a turbine flow meter, a manual flow control valve, and a porous sinter injector into the glass test section. The water flowed down the vertical leg, around the bend, and into the horizontal leg. Water discharged into the supply tank and was recirculated.

Air was introduced into the elbow countercurrent to the water flow. Air, nominally at 760 kPa and 20°C, passed through a bank of rotameters, a heated section, and a manual flow-control valve, before entering the supply tank. Electrical heating tapes were used to bring the air temperature to approximately the same as that of the loop water to achieve adiabatic conditions. Air was injected into the supply tank through a slotted pipe, which extended down the tank. The slots were directed away from the test section inlet to prevent air from impinging directly on the opening. Air proceeded along the glass test section, past the porous sinter and into the separation tank, where it was vented to the atmosphere.

When the loop went into a water carryover mode (see below), a mixture of air and water was carried into the separation tank. The water fell to the bottom of the tank and was drained off, and the air was vented to the atmosphere.

The test procedure was to first establish a preselected, steady water flow in the elbow. The countercflow of air was then increased in small steps until a slug of water formed in the lower leg, eventually leading to water carryover from the elbow. Experiments were performed in which the lower leg of the elbow was slightly inclined, up or down, while the vertical leg was kept in the same position. The uncertainty in the inclination angle of the lower leg was estimated to be ±0.03°.

EXPERIMENTAL OBSERVATIONS AND RESULTS

The experimental conditions studied in this work are summarized in Table 1.

<table>
<thead>
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<td>SUMMARY OF EXPERIMENTAL TEST CONDITIONS</td>
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Pipe I.D. = 51 mm
Radius of curvature of bend = 89 mm
Length-to-diameter ratio of lower leg = 58
Length-to-diameter ratio of vertical leg = 23
Inclination angle of lower leg = +0.35° to -0.25°
Uncertainty in inclination of lower leg = ±0.03°
Pressure = ~ 100 kPa
Water flow rate = 0.04 L/s < ν < 1.0 L/s
(0.1 < $\dot{\nu}$ < 0.9)
Air flow rate = 0 g/s < $\dot{\nu}$ < 8.3 g/s
(0 < $\dot{\nu}$ < 0.5)
Visual Observations of Flooding

Horizontal Case. For completeness, the observations of flooding in an elbow with a horizontal lower leg will be described first (7).

Figure 1 shows the typical flow pattern in an elbow at low water flow rates. Water flows down the vertical leg as an annular film at high velocity. At the elbow, a complex three-dimensional flow pattern results when water drains from the inner side of the elbow to the bottom of the horizontal leg. In the horizontal leg, an abrupt change in water level, called a hydraulic jump, is observed. The flow from the elbow to the jump is supercritical (Froude number > 1). The flow from the jump to the exit of the horizontal pipe is subcritical (Froude number < 1). As the gas-flow rate is increased, the hydraulic jump moves back towards the elbow. At the same time, the air-water interface becomes more wavy, with water occasionally splashing to the top of the horizontal pipe. Eventually, a slight increase in gas-flow causes bridging at the crest of the hydraulic jump forming a slug. The slug is pushed rapidly upstream from the horizontal leg to the vertical leg. The formation of the slug is accompanied by a sharp rise in the pressure drop across the horizontal pipe. The formation of the slug ultimately leads to a continuous carryover of water from the elbow into the separation tank, as shown in Figure 3.

At high water flow rates, no hydraulic jump is observed in the elbow, i.e., the flow is supercritical throughout the horizontal leg. In the present experiment, no hydraulic jump was observed when \( \frac{Q}{d} \) exceeded 0.5. As the gas flow is gradually increased, a slug forms at the end of the horizontal pipe and travels rapidly upstream leading to water carryover from the elbow.

Upward Inclination. Figure 4a shows the typical flow pattern observed in the present experiments with an upwardly inclined lower leg. The phenomena are qualitatively similar to those of the horizontal case (Figure 4b). However, the hydraulic jump location is closer to the elbow and the height of the jump is higher. Flooding starts with the formation of a slug at the enhanced hydraulic jump. It is easy to imagine that if the end of the lower leg of an elbow is raised by one full pipe diameter, the passage of air through the elbow is completely blocked by the accumulated water inside, making countercurrent flow impossible. Thus it is intuitively obvious that slight upward inclinations of the lower leg would cause flooding to occur more easily than when the lower leg is horizontal.

* The Froude number represents a ratio of inertial and gravity forces acting on the fluid and is defined as \( F = \frac{v}{\sqrt{gh}} \), where \( v \) is the mean velocity, \( h \) is the hydraulic depth of water in the channel and \( g \) is the acceleration due to gravity.
Downward Inclination. Figure 4c shows the typical flow pattern in an elbow with a downwardly inclined lower leg. Again, the phenomena observed are qualitatively similar to those of the horizontal case. However, the hydraulic jump location is farther away from the elbow and its height is less pronounced. This is expected since a component of gravity is accelerating the flow and tends to suppress any unstable wave growth. At higher water flow rates, the hydraulic jump becomes so weak that it is hardly observable.

Results

In the following, experimental data are presented in terms of the usual nondimensional parameters \( j_f \) and \( j_c \). First, a comparison between the present data and previous work for an elbow with a horizontal leg is given. Next, the influence of slight upward and downward inclination of the lower leg on the flooding limit is presented.

Horizontal Case. Figure 5 shows a comparison of flooding data from the present experiment with those of Siddiqui et al. (5) for an elbow with a horizontal lower leg. The present data agrees well with those of previous work for \( j_f \) less than 0.55. However, when \( j_f \) exceeds 0.55, the trend of the data becomes different. There is an abrupt increase in the gas flow rate required to cause flooding. This change coincides with the visual observation that in the present experiments, when \( j_f \) exceeds 0.5, the water flow in the horizontal leg of the elbow remains supercritical. This result seems to confirm the reasoning of Wan (7) that the gas flow required to cause flooding in a supercritical flow is higher than that for a subcritical flow since the water level is lower in the former case. Furthermore, when \( j_f \) exceeds 0.5, the flooding location switches from the hydraulic jump to the end of the horizontal leg. Under these conditions, entrance effects probably play an important role in determining the flooding velocity in vertical pipes.

Inclined Cases. Figure 6 summarizes the effect of a slightly inclined lower leg on the flooding limit of an elbow. As may be seen, the flooding limit is sensitive to small changes (<1°) in the inclination angle. An elbow with an upwardly inclined lower leg requires much less air flow to cause flooding than the same elbow with a horizontal leg, whereas the elbow with a downwardly inclined lower leg requires more. Also included in Figure 6 are the inclined elbow data of Siddiqui et al. (5). The qualitative trends of the two data sets are in agreement. The quantitative differences between the data can be attributed to differences in pipe size, length of the lower leg and radius of curvature of the bend.

Figure 6 shows a comparison between the present data and the model calculations of Ardron and Banerjee (6). Good agreement is obtained for the horizontal case when \( j_f \) is less than 0.5. For higher water flow rates, the model is not strictly applicable and hence agreement is poor. Good agreement is obtained for the upwardly inclined case. No comparison is given for the downwardly inclined case since their model is not applicable for this situation.
Effect of Large Inclinations of Lower Leg on Flooding

The present work measured the effect of small inclinations of the lower leg of an elbow on its flooding limit. For large inclinations of the lower leg, no results have been reported.

As the downward inclination of the lower leg of an elbow is increased, the elbow eventually straightens out to a vertical pipe geometry. Consequently, its flooding limit is expected to approach that of a vertical pipe. Some work (9-10) has been reported on the effect of inclination on the flooding limit of a vertical or horizontal pipe. As an initially horizontal pipe is inclined towards the vertical, its flooding limit decreases but does not approach that of a vertical pipe monotoni
cally. At some critical inclination angle, which is system dependent, the flooding limit of an inclined pipe exceeds that of a vertical pipe. That is, below this critical angle, the flooding limit increases with the inclination angle, but above this angle, the flooding limit decreases with the inclination angle. For large inclinations of the lower leg of an elbow, similar observations may be possible.

DISCUSSION

In the event of a LOCA with large stagnation breaks in a CANW reactor, a limit on the rate of core cooling may arise from the occurrence of countercurrent flooding in individual feeders, which consist of vertical, horizontal and inclined sections. If a hydraulic jump is present, the locations at which flooding is likely to occur are the horizontal legs of elbows. The present experiments show that the flooding limit is significantly affected if the lower leg deviates slightly from the horizontal. In particular, the flooding limit is dramatically reduced by a slight upward inclination of the lower leg, and is significantly increased by a slight downward inclination. In other words, emergency coolant could be delivered to the core sooner in the case of a feeder with a downward inclination.

Strictly from the point of view of feeder refilling, vertical and downwardly inclined pipe sections, because of their higher flooding limits, refill faster than horizontal ones. Thus, it would be desirable to design feeder piping to comprise of as many vertical and downwardly inclined sections as possible. Where horizontal runs are unavoidable, it may be possible to design these to maintain a slight downward inclination towards the reactor core. Such a geometry would result in reduced feeder refill times and allow emergency coolant to reach the reactor core sooner. Other ramifications of such a design change must, however, be thoroughly investigated first.

CONCLUSIONS

Based on the present air-water experiments, the flooding limit of an upright elbow is found to be strongly influenced by small changes in the inclination of the lower leg. An elbow with an upwardly inclined lower leg requires much less gas flow to cause flooding than the same elbow with a horizontal leg, whereas an elbow with a downwardly inclined lower leg requires more. The present data are in general agreement with those of Siddiqui et al. (5) and the model calculations of Ardron and Banerjee (6).

ACKNOWLEDGEMENT

The authors would like to acknowledge the help of R.L. Hembrough in carrying out the experiments.

REFERENCES


NUMERICAL SIMULATION OF A CONFINED JET UNDER SUCTION AND COUNTER-MOMENTUM FOR THE CANADIAN MAPLE RESEARCH REACTOR

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ABSTRACT

A new research reactor, called MAPLE (Multipurpose Applied Physics Lattice Experimental), has been developed by Atomic Energy of Canada Limited to generate a maximum thermal output power from 1 to 30 MW. The MAPLE is an upward-flowing, light-water-cooled research reactor with an open-chimney-in-pool arrangement. As part of the thermalhydraulics study, two-dimensional flow behaviour was predicted to study flow patterns in key components of the reactor, in particular the effect of the jet from the core in the open-chimney environment. The study was needed to ensure that the submerged core jet, which contains a high level of radioactivity, would be confined in the chimney. The effectiveness of counter-momentum and suction flows designed to suppress the jet was examined for various conditions.

NOMENCLATURE

a Convection-diffusion coefficient in the finite difference equation
g Gravitational acceleration
p Pressure
p' Pressure correction
S Source term in the finite difference equation
t Time
At Timestep
u Velocity in the x-direction
v Velocity in the y-direction
Vol Control volume
x,y Horizontal and vertical distance

Greek Symbols
µ Viscosity
ρ Fluid density
ϕ General transport variable
Σ Summation

Subscripts
e Effective
l Laminar
nb Neighboring nodes of p in the east, west, north and south direction
p,u Split component of source term
t Turbulent

Superscript
o Old

INTRODUCTION

A multipurpose research reactor called MAPLE has been developed by Atomic Energy of Canada Limited. The MAPLE reactor has an inherently simple design. It is an upward-flowing, light-water-cooled reactor with an open-chimney-in-pool arrangement. The MAPLE class of research reactors is designed to generate a maximum thermal output power ranging from 1 to 30 MW.

The primary heat transport system (PHTS) of a typical MAPLE research reactor contains key components such as the core, chimney, pool, inlet plenum, heat exchangers and pumps. A schematic flow diagram of a typical MAPLE PHTS is shown in Figure 1. During normal operation, heated water from the core flows into the chimney, and is then drawn to the heat exchanger by the pump. The flow is then split into two components - a flow to the inlet plenum and a bypass flow. The bypass flow, a small fraction of the total flow, returns to the top of the chimney and flows downward through it combining with the core flow. The combined bypass and core flows exit from the chimney via an outlet duct leading to the heat exchanger.

The bypass flow is an important aspect of the MAPLE PHTS. It is the countercurrent momentum effect of this flow in the chimney that ensures flow leaving the core does not rise to the chimney top. This is required to ensure that short-lived radionuclides leaving the core do not reach the pool surface, and escape into the environment. The physical behaviour of the core jet in the suction and counter-momentum environment is rather complicated.

No relevant experimental or analytical information for this problem is available in the open literature, although many investigators have dealt with jet problems in stagnant environments, as reviewed in Reference [1].

The present study investigated the effect of the jet on local flow distributions in suction and counter-momentum environments, to determine whether any significant quantities of radionuclides in the jet migrate to the pool surface. The flow patterns in the chimney and pool result from a balance between the inertial momentum of the core jet, the counter-momentum flow down the chimney and the suction flow from the chimney, and also from a turbulent diffusion process through shear action.

MATHEMATICAL MODELLING

Background

In this paper, detailed flow distributions are predicted in the pool, chimney, core and inlet plenum of the MAPLE research reactor. The important geometric conditions that determine flow patterns in the chimney and pool are (a) core diameter, (b) chimney diameter, (c) pool diameter, (d) suction pipe diameter, (e) chimney height, and (f) suction location. Flow patterns in the chimney and pool also depend on the bypass flow ratio (bypass flow/total flow).

The flow equations of mass and momentum conservation were represented in a Cartesian coordinate
system, as shown in Figure 2. The internal walls of the inlet plenum, the core, the calandria and the chimney were modelled as internal boundaries, while the pool water was modelled as enclosed by the pool surface and an external pool wall boundary. The area studied was discretized using a control-volume method, as shown in Figure 2. A non-uniform grid layout of 31 x 38 was used.

A two-dimensional, incompressible flow model [2] was adapted to accommodate the geometry and boundary conditions of the MAPLE research reactor, and to implement a turbulence model.

Conservation Equations

The governing equations for two-dimensional, isothermal, turbulent flow of an incompressible fluid are the conservation equations of mass and momentum. Using Boussinesq's concept of eddy viscosity to model turbulent fluxes of momentum in two-dimensional Cartesian coordinates [3] gives the following conservation equations:

Continuity Equation

\[ \frac{\partial (\rho u)}{\partial x} + \frac{\partial (\rho v)}{\partial y} = 0 \]  

x-Momentum Equation

\[ \frac{\partial}{\partial t} (\rho u) + \frac{\partial (\rho u^2 + \mu_e \frac{\partial u}{\partial x} + \rho \nu \frac{\partial u}{\partial y})}{\partial x} = - \frac{\partial p}{\partial x} + \frac{\partial}{\partial x} (\mu_t \frac{\partial u}{\partial x}) + \frac{\partial}{\partial y} (\mu_t \frac{\partial u}{\partial y}) \]  

y-Momentum Equation

\[ \frac{\partial (\rho v)}{\partial t} + \frac{\partial (\rho uv - \mu_e \frac{\partial v}{\partial x} + \rho \nu \frac{\partial v}{\partial y})}{\partial x} = - \frac{\partial p}{\partial y} + \frac{\partial}{\partial x} (\mu_t \frac{\partial v}{\partial x}) + \frac{\partial}{\partial y} (\mu_t \frac{\partial v}{\partial y}) \]  

The porous-media approach was taken to account for the effect of the fuel assemblies on the flow distributions. The porosity is defined as the ratio of fluid volume to total volume in a control volume. For the problem addressed in this paper, it was more of interest to examine chimney flow patterns resulting from core porosity than core flow patterns themselves. Thus, in the study, an approximate ratio of coolant to core space volume of 0.5 was used to see the effect of the porosity in the core on the chimney flow patterns.

Turbulence Model

Boussinesq's eddy viscosity concept provided the framework for constructing a turbulence model. The turbulent viscosity is not a fluid property but depends on the state of the turbulence. Several turbulence models are available in the literature and a good review of these models can be found elsewhere [4].

Here, we used a simple turbulence model derived from the free jet similarity. The value of effective viscosity was varied from 0.1 to 10 kg/(m·s) to examine its effect on the flow pattern.

The effective viscosity is defined as the sum of the laminar and turbulent viscosities:

\[ \mu_e = \mu_t + \mu_e \]  

Near-Wall Region Model

The wall function method was employed to enforce the logarithmic law near the wall and to model wall shear stress [5]. The first step was to place a node so that it was far enough from the wall for the local turbulent Reynolds number to be much greater than unity. The logarithmic velocity profile for a smooth wall was then assumed to prevail in the region between the wall and this first node from the wall.

Initial and Boundary Conditions

Initial flow velocity fields were set to zero for all the simulations. A converging solution is independent of any specified initial flow velocity field.

To complete the mathematical formulation, boundary conditions had to be specified for all the walls associated with the internal and external structures of the MAPLE research reactor, and for the inlet and outlet flow conditions.

The boundary condition grid layout and specifications are illustrated in Figure 3. Since the external circuit of the MAPLE reactor was not modelled, the inlet and outlet conditions were specified as a mass-momentum source and sink, respectively. A uniform velocity profile at the inlet and outlet was assumed. All the internal walls were modelled such that, by modifying the corresponding convection-diffusion coefficients, the neighboring points across the wall did not influence each other.

NUMERICAL MODELLING

Finite Difference Formulation

The continuity and momentum equations (1) to (3) were combined to obtain the pressure-correction equation that coupled pressure and velocity to satisfy the mass conservation through the SIMPLE procedure [6]. The resulting finite difference form of the pressure-correction and momentum equations for a control volume can be cast into the following general form:

\[ [a_p + \rho (\text{Vol})/\Delta t - S_p] \phi_p = \phi_0 \delta_{\text{nb}} + S_u + \rho (\text{Vol}) \phi'_p/\Delta t \]  

To solve equation (5), the first step was to discretize the region to be modelled into finite control volumes, or nodes, with dependent variables \( u, v \) and \( p \). A staggered grid was used where the velocity nodes were located between the pressure (or other scalar variable) nodes. The convection-diffusion flux terms at control volume faces were approximated using the hybrid upwind-central difference numerical scheme [6] to improve accuracy depending on the local Peclet number.

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The discretized equation (5) is in an implicit expression in terms of dependent variables. The convection-diffusion coefficients of the variables and the source terms (including the pressure gradient term) were evaluated from the previous step. Each equation was solved using an alternating direction method that doublesweep in the \( x \) and \( y \) directions sequentially and then proceeds with the main diagonal matrix algorithm. Since the pressure-correction and momentum equations were solved sequentially in an iterative procedure, the momentum equations were solved based on a previously calculated pressure field. To satisfy the continuity equation, the velocity field used for solving the momentum equations for the next iteration was adjusted by pressure-correction field from the previous iteration.

Convergence

The numerical scheme employed a pressure-correction procedure to solve the hydrodynamic equations. The \( u \) and \( v \) velocities were adjusted by the pressure corrections so that both the momentum and continuity equations were satisfied. This iterative procedure does not always ensure decreasing errors in the equations. When the errors continue to grow, the solution diverges. To reduce any numerical instabilities, under-relaxation in the finite difference method was used.

An iterated solution is completely converged if local values of dependent variables remain unchanged with successive iterations. The local residuals for dependent variables, which are defined as the algebraic difference between the left and right sides of the discretized equation (5), were used as convergence indicators. The convergence criteria for the momentum equation were set as the absolute value of the maximum normalized residuals (i.e., \( \frac{\text{residual}}{\text{residual max}} \)) for the momentum equations being less than \( 10^{-5} \) and the absolute value of the maximum local divergence divided by the mean mass flow through the control volume for the continuity equation being less than \( 10^{-3} \). In addition, a converging trend was judged through the point of maximum normalized residual versus iteration and through the monitored output of the dependent variables at several grid points, to see if the solution oscillated over a wide range of iterations.

The convergence criteria were satisfied for the results. These criteria were considered adequate since the errors caused by machine roundoff and coarse grid size would be larger than the convergence error.

RESULTS AND DISCUSSION

The simulations were limited to steady-state, single-phase flow. Results of some numerical and physical tests are presented and discussed below.

For the vector plots presented, each arrow represents a velocity vector at a given location. For very low velocities, only the arrow tips are shown to indicate their directions. The velocities inside the calandria vessel were set to zero for all runs, although the arrows were shown.

Effect of Suction Velocity

For high-power operation of the MAPLE research reactor, the flow through the core must be increased to keep the fuel cool. The operating limit for high-power operation must ensure that the jet from the core does not escape out of the chimney as a result of the increased inertial momentum. To examine this, the continuity and momentum equations were solved for an effective viscosity of \( 0.0 \text{ kg/(m \cdot s)} \) by varying suction velocity. A fixed porosity of 0.5 in the core was used to account for the effect of the fuel-bundle matrix on the flow pattern.

Figure 4 represents the isothermal flow predictions for three suction velocities with a given effective viscosity. The core-jet flow into the chimney was predicted to be confined within the chimney by a net flow down the chimney. The flow velocities in the upper layer above the chimney were predicted to be equal to the average incoming bypass flow. The bypass flow returning to the top of the chimney thus increases with the suction velocity, and this effectively breaks up the jet in the chimney.

As shown in Figure 4, the flow patterns for the three runs were similar. To explain this, suppose a fluid particle emerged out of the core into the chimney. It will be convected, depending on governing forces acting on it, and diffused by local turbulent energy carried by the particle, which is influenced by its velocity or velocity gradient. A force balance on the particle, including inertial momentum, suction and counter-momentum determines in which direction it will be convected, and it will diffuse off its convected track because of local turbulence. Since the bypass flow ratio to the suction flow was kept the same for the three runs in Figure 4, the flow directions (or the flow patterns) were not expected to change although the magnitudes were.

Effect of Bypass Flow Ratio

Figure 5 shows the effectiveness of the bypass flow in suppressing the core jet for isothermal runs. As the ratio of the bypass flow to the total flow decreases, the core jet shoots higher up the chimney and then, at a ratio less than a critical value, the core jet is no longer confined in the chimney.

Effect of Suction Location

Figure 6 shows the effect of suction location on the core jet. Just as a free jet decays very slowly in a stagnant environment, the core jet decays little up to the suction location. Thus, it appears beneficial from a jet suppression standpoint that the jet be broken up as low as possible by a suction flow and then the remaining momentum of the jet be suppressed by the counter-momentum by the bypass flow.

As shown in Figure 6(b), the counter-momentum near the chimney top is greater than the remaining jet momentum above the suction port. A large recirculating zone by entraining the surrounding pool water in the upper pool layer. The velocities in the upper pool layer, where the jet decays, were predicted to be in the order of \( 10^{-2} \text{ m/s} \). For the highest suction location, a mass balance is maintained near the chimney top. The bypass flow into the chimney returns down the inside walls of the chimney, while the core jet escapes out of the lower region of the chimney.

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Effect of Effective Viscosity

To examine the effect of effective viscosity on the global flow pattern, the effective viscosity was varied.

As shown in Figure 7, as the value of effective viscosity increases, the spreading rate of the jet increases with vertical distance and, thus, penetration of the jet up the chimney diminishes. The inertial momentum of the core jet is dissipated through interactions with the surrounding chimney water, with the bypass flow returning down the chimney and with the suction flow exiting from the chimney. The kinetic energy of the jet is steadily converted into kinetic energy of turbulence. The kinetic energy of turbulence then steadily decays through shear action in the chimney. The higher the effective viscosity, the higher the rate of dissipation and, thus, the less penetration up the chimney.

The effective viscosity range of 0.1 to 10.0 kg/(m·s) establishes the solution range as derived from the free-jet similarity (see Section 2.3).

Effect of Number of Iterations

Figure 8 demonstrates how many iterations are required to obtain a reasonably converging solution for steady-state isothermal flow predictions. A preceding run was made for 20 steps using a time-marching scheme with a timestep of 1 s and then, based on this run, solutions were plotted at 100, 200 and 400 iterations using a timestep of 10^{-4}s. As shown in Figure 8, the solution had already converged in all regions at 100 iterations.

Computation of the convection-dominated flow in the chimney (resulting from the core jet, suction and bypass flow) and in the lower portion of the pool (resulting from bypass flow) is more convergent than that of the diffusion-dominated flow in the upper portion of the pool.

VALIDATION EXPERIMENTS

A scaled model of 1:5 has been built at the Whiteshell Nuclear Research Establishment. The experimental program covers visual injections of jet behaviour and single-phase velocity measurements using hot-film anemometry. Experimental results are useful not only to validate various modelling aspects of multidimensional flow predictions, but also to better understand the flow mechanism affecting jet behaviour under suction and counter-momentum.

The trends of the parametric effects of suction location, suction velocity and bypass flow ratio predicted by the numerical models agree qualitatively with experimental observations. Quantitative experiments are presently being carried out.

CONCLUSIONS

Isothermal flow simulations in two-dimensional Cartesian coordinates have been made to study the performance of the MAPLE research reactor for various conditions. The study examined the effect of suction height, core-jet velocity, counter-momentum flow, and effective viscosity on local flow distributions. The following conclusions may be drawn from the numerical results for isothermal flow:

1. The suction and counter-momentum are the dominant forces that suppress and confine the core jet within the chimney.
2. For a fixed ratio of bypass flow to total flow, the flow pattern in the chimney remains unchanged for the high suction flows.
3. The lower the suction location, the better the core-jet confinement in the chimney.
4. The trends of parametric effects are:
   - the higher the effective viscosity, the more diffused the jet and the less it penetrates up the chimney;
   - the higher the counter-momentum flow ratio to the total flow, the better the core-jet suppression.

The modelling approach can be extended to predict non-isothermal flows and it can also incorporate a more elaborate turbulence model. Although the predicted results require further validation with experimental data, the trends of the numerical predictions of the effects of suction location, suction velocity and bypass flow ratio agree qualitatively with the experimental observations.

The Cartesian two-dimensional representation possesses all the important geometric features, such as core, chimney, bypass line, inlet plenum and pool that are appropriately connected. Some inherent drawbacks in a two-dimensional approximation are that these flow connections do not possess the proper resistances due to a lack of azimuthal flow, and the true geometry cannot be represented in two dimensions. The effect of these cannot be predetermined and so the results may be used qualitatively until they are validated experimentally.

ACKNOWLEDGEMENTS

The authors are grateful to L.N. Carlucci and A.G.L. Holloway of the Engineering Research Branch at Chalk River Research Laboratories for providing their expertise and the original code for the present work.

REFERENCES

FIGURE 1: SCHEMATIC FLOW DIAGRAM OF MAPLE RESEARCH REACTOR PRIMARY HEAT TRANSPORT SYSTEM

FIGURE 2: MODELLED AREA GRID LAYOUT FOR MAPLE RESEARCH REACTOR

FIGURE 3: BOUNDARY CONDITION GRID LAYOUT AND SPECIFICATIONS FOR MAPLE RESEARCH REACTOR
1.8iJ6E*i)0 M/S

(a) SUCTION VELOCITY
OF 2.5 m/s

(b) SUCTION VELOCITY
OF 1.25 m/s

(c) SUCTION VELOCITY
OF 0.5 m/s

FIGURE 4: EFFECT OF SUCTION VELOCITY (CORE POROSITY OF 0.5, BYPASS FLOW RATIO OF 10%
AND EFFECTIVE VISCOSITY OF 1.0 kg/(m·s))

5l F.FFECT OF BYPASS FLOW RATIO (CORE POROSITY OF 0.5, SUCTION VELOCITY OF 0.5 m/s
AND EFFECTIVE VISCOSITY OF 1.0 kg/(m·s))

(a) BYPASS FLOW RATIO
OF 10%

(b) BYPASS FLOW RATIO
OF 5%

(c) BYPASS FLOW RATIO
OF 0%

FIGURE 5: EFFECT OF BYPASS FLOW RATIO (CORE POROSITY OF 0.5, SUCTION VELOCITY OF 0.5 m/s
AND EFFECTIVE VISCOSITY OF 1.0 kg/(m·s))
FIGURE 6: EFFECT OF SUCTION LOCATION (CORE POROSITY OF 0.5, SUCTION VELOCITY OF 0.5 m/s, BYPASS FLOW RATIO OF 10% AND EFFECTIVE VISCOSITY OF 1.0 kg/(m·s))

FIGURE 7: EFFECT OF EFFECTIVE VISCOSITY (CORE POROSITY OF 0.5, SUCTION VELOCITY OF 0.5 m/s AND BYPASS FLOW RATIO OF 10%)
FIGURE 8: EFFECT OF NUMBER OF ITERATIONS (CORE POROSITY OF 0.5, SUCTION VELOCITY OF 0.5 m/s, BYPASS FLOW RATIO OF 10% AND EFFECTIVE VISCOSITY OF 1.0 kg/(m·s))
SESSION I: OPERATIONS

Chairman: P. Stevens-Guille, Ontario Hydro

Load following in Central Nuclear en Embalse: Operating experience and analytical summary.

Steam generator level controllability.
W.G. Schneider and J.T. Boyd, Babcock and Wilcox Canada

Use of acoustic emission to locate the garter springs of a CANDU fuel channel.
N. Badie and A. Sinclair, University of Toronto and H. Licht, AECL CRNL

Leakage from biological shield cooling system in Pickering NGS A.
C.O. Poidevin, E. DiStanislao and J. Mildner, Ontario Hydro
LOAD FOLLOWING IN CENTRAL NUCLEAR EN EMBALSE:
OPERATING EXPERIENCE AND ANALYTICAL SUMMARY

by

J.C. Víñez
National Atomic Energy Commission (C.N.E.A.)
Embalse Nuclear Station
Republic of Argentina

and

H. Keil, A.M. Manzer and J.P. Karger
Atomic Energy of Canada Limited
CANDU Operations
Sheridan Park Research Community
Mississauga, Ontario, Canada
L5K 1B2

AECL and CNEA carried out a joint study to evaluate plant performance during weekly power changes at the Central Nuclear en Embalse. This study is the first opportunity to demonstrate CANDU 600 plant performance under load following operation.

The data collected show that the process and control variables are within design envelopes and control limits of the station. Also, the defect rate of the 10,000 Canadian-built fuel bundles irradiated is only 0.07% which is within the rates experienced at base load CANDU plants of similar design.

In summary, the CANDU 600 plant has been operating successfully with a range of power between 100% to 50% power range and rates up to 20% per hour. The present intention is to extend the study to include daily load changes.

INTRODUCTION

Nuclear reactors, in general, are intended to be operated at continuous high power (base load operation) to take advantage of low nuclear fuel cost. More expensive fossil fuelled generating stations are power cycled to meet the system demand curve. The CANDU reactors in Ontario and New Brunswick follow this base load mode of operation where the grid demands are normally high. However, with the nuclear component of the total electrical generation increasing in many countries, there is an increasing need to use the nuclear option in a load following mode. In fact, CANDU stations have already operated in various degrees of load following at all sites. For instance, in Quebec, Gentilly G.S. is routinely reduced from full power to continuous low power (50%) for several months at a time during spring and summer months when surplus hydro power is available. Outside Canada, CANDU reactors in Pakistan (KANUPP) and India (RAPP) have been operating in load following mode(1,2). While data collection and evaluation have not been extensively reported in the open literature, the tentative conclusion is that CANDUs perform well in the load following mode. No fuel defects or other adverse effects have been reported.

Early in the operating history of Embalse, CNEA identified the need for load following operation to meet the varying load/generation characteristics of their grid. These grid demands require the station to operate in seasonal, weekly and even daily cycles (although CNEA has refrained from daily load cycling operation up to now). Embalse therefore provided an opportunity to assess the load cycling capability of a CANDU 600 in a systematic fashion under actual operating conditions. Thus, AECL and CNEA agreed to participate in a load following study for purposes of addressing the following aspects:

a) How well does the plant control and process equipment maintain the plant within expected operating boundaries?

b) How well is the reactor power distribution controlled to maintain channel and bundle power operating margins?

c) How well does the reference fuel design tolerate the transients associated with load following? Also, are there changes in the consequences of fuel defects?

Load following for the purposes of this paper is limited to the load cycling component; that is, the operator changes the reactor power in response to grid dispatch request. Operation at reduced power levels may then occur for several hours and as long as several days. The other load following component, when the reactor is allowed to follow the turbine demands in response to grid frequency changes, is expected to be evaluated during a future phase of this work.

The data collected and analyzed are based on "routine" periods of predominantly weekly load cycling over one year of operation. The emphasis on data collection was placed on the Primary Heat Transport System, Fuel Management/Core Physics and Fuel Performance. In addition, general data were reviewed on the power history and the grid behaviour. The fuel performance presented in this paper is that related to Canadian-built fuel. This allows a performance comparison between base load and load following operation without the added complication of differences attributable to several fuel suppliers.
The details of the data collection process, design features of the CANDU station that contribute to good load following performance and analytic results are discussed in the following sections.

DESIGN FEATURES

Brief Plant Description

The CANDU PHWR* at Embalse uses heavy water as a moderator and a heat transport medium. Fission heat generated in the reactor core is transferred to the steam generator via two pressurized heat transport (HTS) circuits. Each of these circuits consists of 190 horizontal fuel channels, two steam generators, two coolant pumps and interconnecting piping (Figure 1). The two separate circuits are connected through a common pressurizer, an ion-exchange purification system and interconnecting piping, all of which have valves for isolation.

The reactor consists of the calandria vessel which contains the moderator and the 380 horizontal fuel channel assemblies. Each channel contains 12 fuel bundles. For long term reactivity control, refueling operations are performed on-power using remotely controlled fueling machines to service each end of the fuel channels. During normal refueling, 8 new fuel bundles are inserted at the inlet end of a fuel channel while 8 irradiated fuel bundles are removed at the outlet. The remaining 4 bundles are displaced from the inlet to outlet positions in the channel.

Reactivity control (short term) is accomplished by i) movement of reactivity control units across the calandria boundary and/or ii) direct addition of "poisons" to the moderator. The automatic reactivity control units consist of 21 vertical adjuster rods for shaping the power distribution and for power manoeuvring; 14 vertical liquid (H2O) zone control units for controlling total reactor power and for minimizing flux tilts; and 4 vertical solid cadmium rods for supplementing the liquid zone controllers and for initiating power setbacks. Adjuster rod sequence schemes for power manoeuvring keep flux tilts and fuel bundle overpowers within limits. Poisons in the form of boron and gadolinium salts can be added or removed to augment these reactivity control devices. This "poison addition" action is manually initiated.

CANDU Power Manoeuvring

CANDU reactors have considerable control reactivity available for power manoeuvring. In fact, it is the process and/or equipment which govern the maximum recommended rate of power changes. Table 1 gives the ramp rates for various components within a CANDU plant.

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**TABLE 1**

<table>
<thead>
<tr>
<th>LIMITING ITEM</th>
<th>MAXIMUM RECOMMENDED RAMP RATE</th>
<th>COMMENT</th>
</tr>
</thead>
<tbody>
<tr>
<td>Control System</td>
<td>4%/sec. below 25% power</td>
<td>Pre-programmed computer control rates</td>
</tr>
<tr>
<td>Steam generator level &quot;Swell&quot;</td>
<td>1%/sec. ≥ 25% power</td>
<td></td>
</tr>
<tr>
<td>HTS pressurizer</td>
<td>no restriction up to 90% power 9%/min. above 90% power</td>
<td>To prevent power stepback on high heat transport pressure due to boiling</td>
</tr>
<tr>
<td>Fuel Channel Power Limit (tripping due to flux tilts)</td>
<td>&lt;2%/hour above 80% power</td>
<td>To operate within fuel channel/bundle power limits</td>
</tr>
<tr>
<td>Turbine Trip</td>
<td>1%/min. - cold 10%/min. - hot</td>
<td>To operate within turbine specifications</td>
</tr>
</tbody>
</table>

* An enlarged steam drum in steam generators for newer CANDU 600s generators permit higher ramp rates.

* CANDU Pressurized Heavy Water Reactor

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Fuel Design Features

The standard CANDU 600 fuel bundle contains 37 fuel elements of natural uranium oxide clad in thin Zircaloy sheathing (Figure 2). This design has evolved from extensive irradiations and power reactor experience. Power cycle irradiations were carried out in the NRX reactor. The influence of design variables, such as fuel-sheath gap, voidage, UO₂ density were investigated. More recently, instrumented fuel assemblies have been irradiated enabling continuous monitoring of sheath strain under power cycle conditions. No operating restrictions have been identified for maximum tested ramp rates of up to 150% per minute. These rates are more than 10 times greater than restrictions imposed by process considerations (Table 1). This good performance can be attributed to these design features:

a) graphite CARIM coatings that protect against pellet-clad-interaction,

b) thin ductile sheaths that maintain good heat transfer under power cycling,

c) horizontal orientation that avoids fuel stack relocation effects observed with vertical orientation, and

d) relatively short residence times in-core that minimize the risk of fatigue failure.

EMBALSE OPERATING HISTORY

Power History

The CANDU 600 station at Embalse was declared in service on 1984 January 20. During this first year of commercial operation, the Embalse station experienced about 19 power reductions and 17 trips (Figure 3). The characteristics of the power reductions are summarized on Table 2. Some of the power reductions were in response to grid demands. Other power setbacks and trips were part of initial operating problems, part of warranty tests or were due to operating problems associated with the new plant.

During the second year of operation (1985), there were 45 power reductions. Most of these were in response to grid demands, and 6 were reactor trips (see Table 2). About half of the power reductions started from high power (between 100% and 80% full power). For 2½ months, the reactor experienced weekly load cycles from 80% to approximately 60% power, before returning to full power in September.

<table>
<thead>
<tr>
<th>TABLE 2</th>
</tr>
</thead>
<tbody>
<tr>
<td>SUMMARY OF EMBALSE POWER REDUCTIONS AND TRIPS</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>POWER REDUCTION CHARACTERISTICS</th>
<th>1984</th>
<th>1985</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. POWER REDUCTIONS FROM 81-100% POWER RANGE</td>
<td></td>
<td></td>
</tr>
<tr>
<td>0 - 20% Reduction</td>
<td>4</td>
<td>11</td>
</tr>
<tr>
<td>20 - 40% Reduction</td>
<td>7</td>
<td>14</td>
</tr>
<tr>
<td>≥ 40% Reduction</td>
<td>0</td>
<td>2</td>
</tr>
<tr>
<td>2. POWER REDUCTIONS FROM 61-80% POWER RANGE</td>
<td></td>
<td></td>
</tr>
<tr>
<td>0 - 20% Reduction</td>
<td>6</td>
<td>5</td>
</tr>
<tr>
<td>20 - 40% Reduction</td>
<td>1</td>
<td>7</td>
</tr>
<tr>
<td>3. POWER CYCLES FROM 60% OR LESS RANGE</td>
<td></td>
<td></td>
</tr>
<tr>
<td>1</td>
<td>-</td>
<td></td>
</tr>
<tr>
<td>4. TRIPS</td>
<td>17</td>
<td>6</td>
</tr>
<tr>
<td>TOTAL</td>
<td>36</td>
<td>45</td>
</tr>
</tbody>
</table>

CANDU 600 reactors are equipped with failed fuel detection and location systems that operate on-power. The gaseous fission product monitoring (GFP) system enables the operator to detect a fuel failure and then the Delayed Neutron (DN) system is used to locate the failure. In addition there are methods available for assessing the fuel defect while in-core. Since the channel containing a fuel failure can be refuelled at power via normal procedures it avoids any loss in station capacity factor. The consequences of fission product release can be readily minimized by prompt removal.
1984

% FULL POWER

PLANNED OUTAGE

1985

Jan Feb Mar Apr May Jun Jul Aug Sep Oct Nov Dec

GRID RESTRICTION

FIGURE 3 EMBALSE STATION PERFORMANCE

Fueling History

Normal refueling started in 1984 February, almost one year after first criticality. A Canadian supplier provided the first charge of fuel plus some reload fuel. In 1984 April and May, three fuel channels were loaded with 36 fuel bundles built in the National facility in Argentina. In 1985, during March to June, 460 additional National fuel bundles were loaded into the core. Two other Canadian fuel fabricators also supplied reload fuel in 1985. Summary of the Canadian fuel supplied during these initial three years is given in Table 3.

<table>
<thead>
<tr>
<th>YEAR</th>
<th>INSTALLED</th>
<th>DISMANTLED</th>
<th>DEFECTS</th>
</tr>
</thead>
<tbody>
<tr>
<td>1983</td>
<td>4,600</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>1984</td>
<td>1,800</td>
<td>1,600</td>
<td>3</td>
</tr>
<tr>
<td>1985</td>
<td>1,800</td>
<td>4,200</td>
<td>4</td>
</tr>
<tr>
<td>TOTAL</td>
<td>10,200</td>
<td>6,000</td>
<td>N</td>
</tr>
</tbody>
</table>

* Cumulative defect rate = 0.07%
RESULTS

Plant Behaviour

The process/control behaviour is assessed on the basis of 20 variables that were reviewed for some of the cycles described in the previous section. As expected, the steam generator level, feedwater temperature, reactor outlet header temperature and pressurizer level data all followed linear variations with reactor power (Figure 4). Remaining parameters were found to have small variations (e.g., reactor inlet header temperature changed by only 5°C from 100% power to 60%) or remained constant (steam generator, outlet header and degasser pressures). All these trends confirm the adequacy of the existing reactor components specifications.

At reduced power levels, the control systems are maintaining the parameters within the same tolerance range observed at 100% operation. This observation is made on the basis of 30 minute time intervals. On one occasion, measurements were recorded at 28 second intervals (see Figure 5 for reactor power). The power manoeuvring consisted of a series of steps (both up and down) giving an average rate of 0.33% per minute. The mean power and corresponding process values are similar to those at steady state i.e., Figure 4.

![Graph 1: Steam Generator Level vs Reactor Power](image1)

![Graph 2: Feedwater Temp vs Reactor Power](image2)

![Graph 3: Reactor Outlet Header Temp vs Reactor Power](image3)

![Graph 4: Pressurizer Level vs Reactor Power](image4)

![Graph 5: Reactor Power Changes](image5)

**Figure 4** Embalse Plant Response to Reactor Power Changes

**Figure 5** Reactor Power Changes at Embalse
Fuel Performance

The Canadian supplied fuel had a low defect rate of 0.07% of bundles irradiated (Table 3). This defect rate is below the 0.10% average rate previously reported for CANDU fuel used in baseload stations (8). Overall, there were seven suspected defective Canadian-built fuel bundles.

Two of the seven bundles were visually confirmed in the inspection bay as having been defective (Table 4). Each contained only one defective fuel element per bundle. The other five were labelled as defective based on delayed neutron data indications and on activity released during fuel removal. The limited success in finding defect damage is likely due to the nature of the defect, i.e., small amount of visible damage, and/or due to limitations of the inspection equipment. Of these seven failures, six belonged to first charge fuel which were subsequently removed from the core by early 1985, in the early stages of the load following study. Only one fuel bundle is confirmed as having failed during the load following period in 1985. One of the 18 outer fuel elements displayed hydride damage at the upstream end. While this isolated fuel defect, occurring in Channel S14, is not regarded as statistically significant, it was nevertheless inspected in detail.

The power history of the defective bundle from Channel S14 is shown on Figure 6. The defect was preconditioned at high power to an average burnup of 60 MWh/kgU, power cycled at reduced power (weekly cycles), and returned to high power conditions on September 9 when the reactor power increased from 60 to 100% power. The ramp rate was about 0.2 %/min to 92% power while full power was achieved 10 hours later. After a few days at full power, S14 was identified as a suspect channel by the DN system and subsequently refuelled on October 24. The time of fuel failure appeared to have coincided with the reactor power increase on September 9.

The bundle power history data were analysed to determine if conditions were more severe for the S14 defect than for other bundles in the core. Power cycling may cause a higher portion of the fission products to concentrate in the available gap between the UO2 pellet and sheath. Thermal effects on the pellet may also cause higher sheath stresses. Both changes tend to favour conditions conducive to stress corrosion cracking (SCC). Previous experience has indicated that high burnup fuel with the highest ramped power (P) and/or highest increase in power (ΔP) is more susceptible to SCC (10).

| TABLE 4 |
| --- | --- | --- | --- | --- | --- |
| Fuel Channel | Loading Date | Shifted Date | Discharge Date | Average Burnup (%) | Status |
| R09 | F/C | 85.03.19 | 85.10.24 | 96 | position 7 |
| R18 | F/C | 84.04.11 | 84.07.06 | 41 | position 10 |
| Q14 | F/C | 84.03.29 | 85.02.20 | 80 | positions 6 or 7 |
| R16 | F/C | 84.03.29 | 85.02.20 | 80 | positions 6 or 7 |
| S14 | 85.03.19 | 85.10.24 | 96 | position 7 |

F/C - first charge fuel
* - fuel shuffled from channel R17
The ramped power of the S14 suspect (50 kW/m for the outer fuel elements) was close to the maximum value in the core on September 9 (Figure 7). It was also within the high power envelope. This envelope outlines the highest bundle powers in the core for 100% base load operation (peaks at 900 kW bundle power or 57 kW/m element linear power). The envelope allows for refuelling effects associated with the reference 8 bundle shifts and encompasses about 99% of the bundle power histories. Some bundles may exceed this envelope temporarily due to an unusual fuelling operation, or short term reactivity control transient. Although the reference fuel has been designed to operate without failure within this envelope, good performance has been demonstrated well above this curve by several irradiations at Chalk River.

The increase in power of the S14 suspect (17 kW/m for outer fuel elements) was also close to the maximum %' experienced by other bundles in the core during the reactor power increase (Figure 8).

The fuel management data from Embalse show that about 8 fuel bundles had similar or higher burnups and had both P and APs greater than the S14 defect. However, no failures were observed among these bundles with more severe power histories than the S14 defect. In absence of detailed fuel examinations, it is therefore concluded that load following operation did not directly cause the S14 bundle to fail since other bundles survived more severe power histories. Also, only one fuel element failed among the 18 outer fuel elements that experienced identical power histories.

**FUTURE ACTIVITIES**

The study may be expanded to include a wider range of operating conditions. In addition, the effect of increased load cycles on "balance of plant" equipment may need to be evaluated, particularly if the power reductions at Embalse are associated with routine operation of turbine steam bypass to the condenser. Additional "finer" time printouts of some selected transients may also be useful to evaluate control system dynamics, particularly for faster power cycling.

Additional fuel operating experience, particularly at higher power ramp rates and more frequent power cycles would help expand the data base on CANDU power reactor fuel.

**CONCLUSIONS**

The Embalse station has been operating successfully with a range of load following power changes within the 100% to 50% power range. The CANDU 600 design has demonstrated promising capability for this mode of operation.

The Canadian supplied 37-element CANDU fuel has a lower fuel defect rate at Embalse (0.07%) than at sister plants that operate under base load conditions. This low defect rate, combined with the absence of multiple element failures suggests that the fuel duty cycle at Embalse is no more severe than at base load stations.
REFERENCES


ABSTRACT

The steam generators used in the CANDU PHWR system are exposed to operating transients considerably more rapidly than those in PWR systems. These transients include power increases at rates as high as 1% per second and rapid power run-back from full load to 60%. The paper reviews the development of the water level control philosophy for the CANDU steam generators. In particular, it compares the design and commissioning experience of the Point Lepreau G.S. and the Bruce G.S. "H" units and discusses the application of that experience to the design of subsequent units.

INTRODUCTION

This paper compares the design and operating experience related to the water level control of the steam generators at the Point Lepreau plant of New Brunswick Electric Power Commission at the four unit Bruce "H" plant of Ontario Hydro. These units are similar in many ways but have a marked difference in water level controllability. Both the Point Lepreau and the Bruce "H" Unit 5 and 6 reactors have been fully commissioned and are now in commercial operation and performing very well.

STEAM GENERATOR CONFIGURATIONS

The steam generators at Point Lepreau and Bruce "H" are two of the 9 designs developed by Babcock & Wilcox Canada (BWC) for the CANDU nuclear program. These units are listed in Table 1. These range from the NPD units which went into service in 1962 through to the Cernavoda 630 MWe net units presently under construction. These designs are shown in Figure 1 (except for the horizontal shell NPD units). Of the 203 steam generators contracted for, 112 are presently in operation. These units have had an excellent record of reliability. For instance, of the 472,700 tubes now in service there have been a total of 63 tube failures of which all but 13 were in the first unit at NPD.

The Point Lepreau and Bruce "H" units are shown in Figures 2 and 3 respectively. They are similar in that both are vertical Recirculating Steam Generators (RSG's) with integral steam drums containing the same steam separators and with the same type of tube/tube support arrangement.

At Point Lepreau four steam generators serve the reactor which has a 640 MWe net electrical capacity. Each steam generator produces 261.6 kg/sec (2,076,000 pph) of steam flow at 4,688 Mpa (680 psia). These units have integral preheaters. The basic shape of the Point Lepreau units results in a number of features which significantly affect water level control. The desire at the design stage for extra high circulation led to an unusually long riser. The drum length on the other hand is relatively short - a fact which limits the ability to accommodate the level control effects of the large riser. Nevertheless this drum was consistent with the design practice at the time and is certainly adequate in terms of steady-state operation.

At the Bruce "H" plant there are four reactors of 750 MWe net capacity. Each has eight steam generators, each producing 163.8 kg/sec (1,300,000 pph) of steam at 4.275 MPa (635 psia). The feedwater enters the drum only slightly subcooled after being preheated by separate preheater units (primary coolant to feedwater heat exchangers). These steam generators have however been considerably optimized with respect to level control. By comparison with the Point Lepreau units it can be seen that the risers are much shorter, which minimizes the amount of inventory displaced by void on power run-up. Also, they have a lengthened drum which enhances the ability to accommodate such void. Table 2 provides a volume comparison which shows the absolute and relative magnitude of the riser and drum volumes. "Drum volume" refers to the drum volume up to the normal full power water level, net of all separator riser and metal volumes and of all volumes below the primary separator deck. It is this drum volume which contains the water level and which can therefore absorb changes in boiler water inventory and in riser void fraction.

CANDU WATER LEVEL CONTROL CONCEPTS

The ability to maneuver quickly has always been inherent in the CANDU concept; the reasons relate to poison avoidance and to the configuration of the reactor. The sensitivity to "poison out" results from the use of natural uranium fuel. The reactor must be restored to a high power level very quickly after a trip (typically within about forty minutes) to avoid xenon build-up; otherwise the reactor will be down for about 48 hours with the attendant loss of revenue. Regarding the reactor configuration, the reactor system consists primarily of relatively light weight components which can easily accommodate rapid start-up and maneuvering cycles.

<table>
<thead>
<tr>
<th>Plant</th>
<th>MWe (net)</th>
<th>In-Service</th>
<th>No. of R.G.'s</th>
</tr>
</thead>
<tbody>
<tr>
<td>NPD</td>
<td>640</td>
<td>1962</td>
<td>1</td>
</tr>
<tr>
<td>Pickering A (1-4)</td>
<td>4 x 515</td>
<td>1971-1</td>
<td>4 x 12</td>
</tr>
<tr>
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<td>125</td>
<td>1972</td>
<td>4</td>
</tr>
<tr>
<td>Bruce A (1-4)</td>
<td>4 x 750</td>
<td>1972-4</td>
<td>4 x 8</td>
</tr>
<tr>
<td>Gentilly 2</td>
<td>640</td>
<td>1981</td>
<td>4</td>
</tr>
<tr>
<td>P. Lepreau</td>
<td>640</td>
<td>1981</td>
<td>4</td>
</tr>
<tr>
<td>Embalse</td>
<td>640</td>
<td>1983</td>
<td>4</td>
</tr>
<tr>
<td>Pickering B (5-8)</td>
<td>4 x 515</td>
<td>1983-6</td>
<td>4 x 12</td>
</tr>
<tr>
<td>Bruce B (5-8)</td>
<td>4 x 750</td>
<td>1984-7</td>
<td>4 x 8</td>
</tr>
<tr>
<td>Darlington (1-4)</td>
<td>4 x 480</td>
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<tr>
<td>Cernavoda (1,2)</td>
<td>2 x 440</td>
<td>1968-80</td>
<td>2 x 4</td>
</tr>
</tbody>
</table>

203
The principal power maneuvering cycles of most interest to this discussion are:
- **Rapid Start-Up**: a rapid power increase from about 22 to 100% including a power increase of 12 per second from 22% to 80% power.
- **Load Rejection**: a run-back of reactor power from 100% to 60% power at 12 per second subsequent to loss of the electrical line load.

The rapid start-up requirement was introduced to the design requirements at the latter part of the concept stage for the Point Lepreau units. The difficulty of dealing with this transient was not fully appreciated until detailed analyses demonstrated...
that the only way to start-up at the very rapid rate specified was to set the zero power water level at a point in the downcomer i.e. below the primary deck. This expedient proved difficult in terms of level response analysis and level control design. For instance a "medium range" level measurement scheme had to be introduced for loads below 25%. This necessitated switching from the medium to the normal narrow range level measurement system during power run-up. It also required a correction to the medium range measurement to allow for the dynamic effects of downcomer velocity. Provision in the operating instructions and commissioning procedure was therefore also required for confirming the level correction factor, for handling the switch over between signals, etc. As we will see, this proved bothersome.

A typical CANDU water level control diagram (in this case the Point Lepreau diagram) is shown in Figure 4 and the control system is shown in Figure 5. In normal operation, the level is operated to the "Control Line", which is variable with power. This variable level offsets the increase in riser void and thus reduces the level increase or "swell". The level is controlled by the feed valve as set by the level controller which is fed by a three component signal in which the level error signal is blended based on steam and feed flow. Note also that the controller must accommodate many upset situations in addition to normal maneuvering so that its sensitivity to normal signals may be somewhat exaggerated.

In addition to the control line, the level diagram features high and low level alarm lines and a number of trips. At the maximum acceptable level we have a constant level (with power) turbine trip line to protect the turbine against high moisture carryover. Below the low level alarm line we have a stepback line which powers the reactor back to zero power at 17 per second. Below the stepback is the SDS1 line which trips the reactor control rods via independent circuitry. Below SDS1 is the SDS2 line, which trips the reactor by the injection of gadolinium nitrate directly into the heavy water moderator pool surrounding the fuel channels.

### Point Lepreau Commissioning Experience

Typically, the water level control commissioning work in a CANDU plant proceeds as follows. At cold and zero power hot conditions, the instrumentation is checked out and calibrated. As the reactor operating license is raised to the low power range, a series of limited power run-up and level sensitivity checks are performed. At higher powers these continue. As the license approaches full power, full run-up tests are done and load rejection and other types of transients are executed.

Point Lepreau was the lead plant in terms of commissioning progress of the three early 600 MWe series CANDU units. The commissioning work was therefore conducted with extra care, particularly in the lower power ranges. As indicated by R.M. Crawford of NBRPC, (Ref. 1), the start-up work at Point Lepreau was ultimately successful in achieving the various transients without acceptable level variation, but only after a significant amount of commissioning effort.

Some of the water level commissioning experiences at Point Lepreau, as reported in Reference 2, were as follows. The first observation of a dramatic water level response was during a zero power simulation of crash cooldown. During this test, the 1030 KPA (150 psi) reduction in boiler pressure yielded a level increase of 3.7 meters (145 inches). This demonstrat-
Another effect seen at low power was the sensitivity to feed flow. At 10% power, for example, the feed flow may easily fluctuate between 0% and 35%, simply as a consequence of the action of the level control system. Further, at any reactor power level, the heat transport is inherently such as to first raise the feedwater to saturation temperature; any additional heat is then available for generation of steam. It therefore transpires that during operation at 10% power a feedwater flow fluctuation between 0% and 35% results in a corresponding variation of steam flow between 12% and 0% respectively.

Obviously such a change in steam flow creates an attendant large variation in water level due to the variation in void within the unit. A manifestation of this effect is seen in the situation where a large feed flow is introduced to correct a low level. The large feed flow tends to suppress the void and thus the level. When the level is finally restored, the feed flow is cut back but by this time the inventory is excessive and the level overshoots. This effect is particularly noticeable at low powers but can be significant at all power levels unless the level control system is tuned to minimize the resultant level overshoot.

As noted above, the requirement for rapid restart resulted in a zero power water level which was within the downcomer. This proved to be a very awkward feature. Dynamic effects were such that during the rapid power run-up, the water level in the narrow downcomer initially took a dramatic dip as shown in Figure 6. These dips resulted in a number of reactor stepbacks to zero power. As the rapid start-up progressed to higher power levels, the boiler void increased as expected. The resultant water level

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**Fig. 6:** Point Lepreau Rapid Power Run-Up

**Fig. 7:** Point Lepreau Load Rejection - Observed Conditions

**Fig. 8:** Point Lepreau Load Rejection - Water Level Simulation
swell, as also shown in Figure 6, was such as to risk a turbine trip on high water level at mid power. Since the above rapid start-up cycle was made with zero feed flow, the reduction of inventory by steam production eventually caused the level to drop back to the control line. To accommodate all of these effects, the station ultimately decided to start the run-up at a slightly higher level and to do a "hold" at about one third power. The hold allows the water level to decline to the control line such that the balance of the power increase can be completed with the level following the control line.

Achievement of a successful load rejection cycle was also an area of difficulty for the commissioning staff. The requirement is to successfully reduce power to 60% subsequent to a turbine trip and to operate stably at that power (with steam bypass to the condenser) until a restoration of load and a return to full power can be executed. The result of an early trial is shown in Figure 7 which shows the observed operating pressure, water level, level setpoint and stepback level versus time. After several oscillations, the level dropped severely, a stepback was incurred and the power and the level dropped away. Efforts to analyze this showed that the level variations were not due to inventory variation. The variation was ultimately stimulated in a rather simplistic manner by tracking the effect of the rather sudden pressure changes on void levels. If we visualize a sudden step reduction in boiler pressure, we see that the water throughout the unit, including that in the drum, is flashed to the new lower pressure condition. The void thus created stays with the water until it passes through the separators at which point the void is released from the recirculating fluid. Because of the relative slowness of the circulation cycle, it takes a number of seconds (typically a mean time of about 7 sec.) for such newly created void to be released. During this time the level will be temporarily high. This sudden pressure change effect is quite dramatic (especially in a unit with a small net drum volume), and applies to pressure increases as well as decreases. Further, the effect totally disappears after one cycle of the circulating fluid. Figure 8 shows the results of the simulation of the above transient based on this effect. The figure shows the predicted level based on "steady-state" circulation (void only affected by power and specific volume) as well as the "transient" prediction incorporating the sudden pressure change effect. After seeing this sensitivity to sudden pressure changes, the commissioning staff were able to improve the operation of the CSBV valves so as to minimize the pressure fluctuations and thus achieve a successful load rejection cycle (the Condenser Steam Discharge Valves control steam bypass to the condenser and thus control drum pressure).

The final result of the above efforts was that the boiler level control commissioning was completed and all of the necessary maneuvers including rapid run-up and load rejection, were demonstrated and incorporated into operating practice. The plant commissioning was completed in February 1983 and it has operated well since that time.

BRUCE COMMISSIONING EXPERIENCE

By contrast, the water level commissioning experience at Bruce "B" was orderly and in line with expectations. Two of the principal commissioning runs and their results were as follows.

The power run-up test results are shown in Figure 9 which is based on the actual results taken by the plant's own computerized control and data collection
system. These are for a power increase at 0.4% per second from 107% to 60% power. The level results show an increase in level in boilers 1 and 2 of about 1.0 metre, which generally follows the level set point as demanded by the control diagram. The level does not overshoot and it quickly settles at the required value for the new power condition. The level does dip somewhat - a fact which is attributed to the sudden influx of cooler feedwater since the dip corresponds to the maximum opening of the level control valve. Also the level has a slight ongoing fluctuation which is attributed to the CSDV operation. While this cycle does not cover the full run-up power span or the rate as specified, it does show a very satisfactory type of level response.

The level response during a load rejection cycle is shown in Figure 10. This cycle involved a turbine trip at full power and the subsequent step back of power to 65%. In addition to the change of power level, this cycle involves rapid closure of the main steam stop valves and assumption of control by the CSDV's of the steam flow and the pressure. The operating pressure is controlled to a constant value during this and all other load changes. The resultant level response very quickly stabilizes at the 0.5 m lower value demanded by the level control setpoint. The level undershoots by about 0.3 m which ties in with a temporarily high feed flow. It is possible that the level undershoot and feed flow overshoot also relate to a temporary overshot in pressure but this has not been confirmed.

**SUBSEQUENT UNITS**

Subsequent to the design of the Point Lepreau and Bruce units, steam generators have been designed for the Darlington plant of Ontario Hydro and for the Cernavoda plant in Romania. The 880 MWe net Darlington plant incorporates 4 steam generators in each of the 4 reactors. These units have 4831 m² (52000 ft²) of heat transfer surface and weigh 330 tonnes (168 tons). The Cernavoda reactors are the same as those at Point Lepreau and the steam generators have the same loading and overall size but different steam drums and tube supports (both Darlington and Cernavoda use lattice type tube supports and CAP (Curved Arm Primary) steam separators).

The Darlington steam drums were sized in a highly conservative manner based on specified requirements. They feature level control to a constant inventory, inclusion of all alarm and trip lines at all power levels within the drum range and margins for all possible instrument errors. These units should have no problem with level control, however, the design is somewhat conservative and comment will therefore be directed to the Cernavoda units which are more optimum and also relate directly to the Point Lepreau application.

The Cernavoda steam generators have the same performance requirements and the same tube surface and overall vessel length as the Point Lepreau units but the drum and vessel volumes have been optimized significantly. The drum is considerably larger than that of the riser smaller and the drum volume larger even though it is smaller in diameter. A further major improvement in the drum versus riser volume is achieved by the use of the CAP separators. These separators make much more efficient use of space in that they have a minimal riser volume leaving maximum net area on the downcomer side for absorbing swell or inventory changes. Furthermore, these separators are able to work efficiently over an extremely broad water level range - a feature which allows the advantageous constant inventory level control concept to be used as discussed below. The impact of these changes can be seen in Table 2, which compares the relative size of the riser and drum volumes for Point Lepreau, Bruce, and Cernavoda. Clearly the Cernavoda units will have a greatly enhanced ability to accommodate variations in riser void.

The principal benefit of these improvements is to allow water level to be controlled to a constant inventory scheme. With this method, the level control line varies with power so that the inventory is the same at all power levels. With this type of control scheme, the feed and steam flows can vary in unison in response to sudden load change. This greatly simplifies the control concept. It avoids temporary level collapse or swell due to an excess or deficiency of feed flow. It avoids the need for power run-up with zero feed flow - a process which is not easy to implement and which also causes thermal shock to the equipment due to sudden changes in feed flow. Furthermore, this approach allows all of the water level alarm and trip lines (with the exception of the low power end of the SDR1 and SDB2 lines) to be within the normal drum measurement range. The overall result is a simple, effective control concept based almost entirely on the normal drum level measurements.

**CONCLUSIONS**

In view of these experiences a number of conclusions can be drawn:
- attention to the boiler level control requirements must begin at the earliest stages of boiler and plant design concept evolution
- optimum design is one which allows operation with a constant boiler water inventory throughout the entire load range. This allows feed flow to vary in unison with steam flow during any load change
- operation with level outside of the normal drum range (i.e. in the downcomer) is very undesirable - even at low power levels
- design features which enhance controllability are: high circulation ratio, small riser capacity, large drum capacity in terms of net area and length, and efficient steam separators with broad water level range capability.

**ACKNOWLEDGMENT**

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**REFERENCES**


USE OF ACOUSTIC EMISSION TO LOCATE THE GARTER SPRINGS OF A CANDU FUEL CHANNEL

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ABSTRACT

A potential method for determining the locations of garter springs in a CANDU fuel channel, without defueling the channel, is described. The method is based on detection of sound waves transmitted along the pressure tube when the garter springs are made to "bounce" on the tube. Experimental results from a fuel channel mock-up are presented, together with analytical work that attempts to model sound propagation in a fuel channel. The direction of future work on this topic is also outlined.

BACKGROUND

The investigation into the cracking of pressure tube G16 at Pickering Unit 2 revealed that physical contact had been made between the pressure tube and calandria tube. This contact established a large thermal gradient in the pressure tube, which led to an accumulation of hydrogen at the contact points, and the build up of brittle hydride blisters. This sequence of events was possible by the displacement of the fuel channel's garter springs from their prescribed locations.

Both Pickering Units 1 and 2 used Zr-II pressure tubes, a material which prone to hydrogen pickup via a corrosion reaction with the coolant. These reactors are currently being retubed with a Zr-2.5% Nb alloy, used in all later CANDU reactors. Tests have indicated that hydrogen pickup by this alloy is extremely slow, so that hydride cracking may no longer be a concern. Nevertheless, it is desirable to confirm that all garter springs in operating reactors are in their design locations, so that pressure tube/calandria tube contact cannot take place.

AECL and Ontario Hydro have developed several tools to locate a fuel channel's garter springs (and reproduction them if required). Unfortunately, the present procedure is time-consuming, and requires that there be no fuel in the channel. It is quite impractical to inspect a fully-commissioned reactor in this manner; the task of emptying several hundred fuel channels, performing the inspection, and reloading the fuel would result in months of downtime.

The goal of this project, therefore, is to develop a method to locate the garter springs in a CANDU fuel channel, without removing the fuel. The "ideal" system, from a reactor operations standpoint, would have the following characteristics:

1. The system would locate all garter springs (either 2 or 4) in a fuel channel, with a maximum uncertainty of ±10 cm.
2. Any measurements would be made either very quickly or on power.
3. Results would be obtained immediately.
4. Minimum disruption would result to the integrity of the fuel channel. This rules out the breaking of welds, introduction of foreign substances to the channel annulus or calandria, etc.

SYSTEM DESIGN

The inner cylinder (pressure tube) of a CANDU fuel channel contains \(^{19}F\) fuel and \(D_{2}O\) pressurized to approximately 10 MPa. Either 2 or 4 garter springs prevent contact between the hot pressure tube and the relatively cool calandria tube. The annular space between the tubes, where the garter springs are located, is inaccessible to a conventional inspection probe, and the outside of the calandria tube could be reached only with extreme difficulty. The only part of the fuel channel for which there is relatively good access is the outer portion of the endfittings (past the Graylow feeder pipe connection).

Acoustic emission is one of the potential methods identified to locate the garter springs without defueling. In principle, sensors mounted on a fuel channel's endfittings could detect vibrations originating from impingement of the garter springs on the pressure tube. When the latter is excited by a "shaking" mechanism or a sharp blow, analysis of these vibrations would indicate the locations of the garter springs.

In practice, the fuel channel has a hideously complicated geometry with ill-defined boundary conditions and damping mechanisms. Even more disturbing are indications that each fuel channel is to some degree acoustically unique - subtle differences in the rolled joints, contact pressure between garter springs and pressure tubes, pressure tube uniformity, amount of creep, boundary conditions, etc., may make it impossible to describe acoustic waveforms in a "typical" fuel channel.

Characteristics of Sound Propagation

When a pressure tube endfitting is subjected to a sharp blow, exciting the garter springs to impact on the pressure tube, sound waves radiate outward from the
impact point. Assuming the tube to be a perfect thin-walled cylindrical shell, these waves are amenable to analytical treatment. Such waves could be studied experimentally without great difficulty if the tube were of infinite extent. Certain favoured frequencies would be detectable as there would be constructive interference at these frequencies among waves spiralling around the shell; if the frequency band were sufficiently high, there would also be interference among waves bouncing between the inner and outer face of the shell, resulting in Lamb-type modes. These waves would be dispersive, so that the waveform would become more and more spread out and ill-defined the further from the source one measured the waveforms.

In our case, the pressure tube is not of infinite extent. The waveform must pass through the rolled joint and down the endfitting before it can be measured. The geometries of the joint and endfitting are not amenable to analytical treatment, so that it is difficult to quantify the distortion expected on the waveform. In theory, however, both ends of the fuel channel are similar, so that direct timing of the waveform at each endfitting, coupled with knowledge on wave propagation in the pressure tube, could indicate the location of the garter springs.

Measuring the wave propagation characteristics in the pressure tube however, is greatly complicated by the finite length of the tube and its complex boundary conditions. Waves will now bounce back and forth along the tube (and in the endfitting) creating axially standing waves. Any attempt to measure the frequency spectrum of the waveform will reflect that the boundary conditions and length of the tube favour certain frequencies, while other frequencies will be attenuated. This will be more and more apparent the longer the signal sampling time. However, these axially standing waves can not directly indicate the garter spring locations. Based on the tube dimensions, lower order beam modes are expected to be present, although resonant frequencies cannot be accurately predicted due to lack of information on the boundary conditions.

### Signal Processing Strategies

The above observations suggest methods by which acoustic emission might be used to locate garter springs:

1. **Direct Timing of Wave Pulse:** This method has been investigated at Ontario Hydro. Due to the low attenuation of waves in the fuel channel, this method is complicated by internal reflections which distort the wave. The goal is to find a non-dispersive mode of propagation that is relatively fast, so that a sharp waveform can be observed. Measurement of the difference in arrival time of the waveform at each endfitting would then indicate the location of the emission source, i.e., the garter spring.

   Low frequencies of a few hundred to a few thousand hertz are the most promising. This method has the advantages that at low frequencies there are relatively few resonant modes to interfere with each other, and equipment need not be as sophisticated as for high frequencies. Resolution may be a problem, however, due to the relatively long wavelength.

2. **Perturbation Analysis:** It may be possible to determine the garter spring locations by measuring their perturbing effect on the fuel channel’s frequency spectrum. Such perturbations, however, would be a strong function of the degree of pinching experienced by each garter spring between the pressure tube and calandria tube. This analysis method is also being studied by Ontario Hydro Research.

### Experimental Apparatus

A partial fuel channel mock-up, consisting of a pressure tube and one endfitting, was assembled at the Nondestructive Testing Development Branch of Chalk River Nuclear Laboratories (CRNL), as shown in Figure 1. Both ends of the tube were capped so that it could be filled with water. Properties of the pressure tube are listed in Table 1. Several preliminary experiments were

<table>
<thead>
<tr>
<th>TABLE 1: PROPERTIES OF Zr-2.5% Nb</th>
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<tbody>
<tr>
<td><strong>Property</strong></td>
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<tr>
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<tr>
<td>Tube Length ( l )</td>
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<tr>
<td>Tube Thickness ( h )</td>
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<tr>
<td>Tube Mean Radius ( a )</td>
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<tr>
<td>Density ( p )</td>
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<tr>
<td>( C_t )</td>
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<tr>
<td>( C_L )</td>
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</table>

Based on Data of Reference 11.
conducted and analyzed at CRNL to optimize the system and set parameters on equipment. Waveforms were picked up by an accelerometer which passed the signals through a buffer and filter to a digitizer. Permanent record of the signals were stored by a tape deck in records of 2018 8-bit data points. The digitizer was triggered by a signal from an ultrasonic probe, placed close to the point of source impact.

The impact source was located at the 12 o'clock position of the tube, and could be moved from D = 17 cm up to D ≈ 500 cm, (see Figure 1). The accelerometer was mounted on the endfitting endplate, but could be moved circumferentially around the plate to various angles θ.

**SIGNAL ANALYSIS**

A number of sets of measurements were performed on the fuel channel mock-up; where possible, a single variable was isolated for detailed study. The goal of these experiments was to determine whether a garter spring located at a certain point on a pressure tube has a characteristic, repeatable signature with a spatial resolution of 10 cm or better. In particular, optimum values were sought for the following parameters:

(a) digitization rate (μs/sample point)
(b) signature length T(μs)
(c) pressure tube mesh (spatial density of library signatures required to represent all pertinent garter spring locations. This is closely related to the system resolution).

In addition, information was acquired on signal repeatability and velocity.

Two different impingement sources were examined for time and frequency domain characteristics. One source was a garter spring manipulated by an electromagnetic system; the second was a 1/4-inch steel ball dropped on the tube. The resulting waveforms, measured on the endfitting, were visually very similar, and had similar rise, duration, and fall times. This indicated that it is the fuel channel, rather than source type, that predominantly affects signal patterns. All future experiments therefore used an automated device to drop steel balls on the pressure tube for an impingement source, rather than the more cumbersome "shaking" of a garter spring.

![FIGURE 1: EXPERIMENTAL EQUIPMENT](image)

**FIGURE 1: EXPERIMENTAL EQUIPMENT**

Two different impingement sources were examined for time and frequency domain characteristics. One source was a garter spring manipulated by an electromagnetic system; the second was a 1/4-inch steel ball dropped on the tube. The resulting waveforms, measured on the endfitting, were visually very similar, and had similar rise, duration, and fall times. This indicated that it is the fuel channel, rather than source type, that predominantly affects signal patterns. All future experiments therefore used an automated device to drop steel balls on the pressure tube for an impingement source, rather than the more cumbersome "shaking" of a garter spring.

![FIGURE 2: ACOUSTIC SIGNAL FROM IMPACT SOURCE LOCATED AT D = 67 cm](image)
Signals were observed to have a complex pattern of pulses, the first pulse lacking any sharp front that could be used for timing. As mentioned earlier, it is believed that these wave packets are due to various modes of propagation and multiple reflections in the endfitting. To date, the various pulses have not been individually identified.

The captured signals illustrated several dominant features of the waveforms. The signal shown in Figure 2a (representing D = 67 cm), shows a clearly-defined sequence of pulses arriving at the accelerometer and the very slow attenuation of the signal strength with time. Many of these pulses are due to reflections at the end of an endfitting or pressure tube; identifying each pulse would be an awesome task.

The frequency domain representation of this signal is shown in Figure 2b; many sharp resonance peaks are evident. These peaks shift slightly both in frequency and relative magnitude as the signature sampling time T or space-to-endfitting distance D is altered; this is due primarily to the frequency dependence of the attenuation. The following trends were observed:

1. The frequency increment between adjacent major resonances is fairly constant at about 5.1 kHz (Figure 2). This regularity could be caused by the resonances each being a different order of some fundamental mode.
2. As D is increased, the lower frequency resonances are in general more highly attenuated than high frequency resonances.
3. Related to item #2, signals with a large value of D tend to have a broader signal front, i.e., a less well-defined arrival time of the initial pulse. This phenomenon, largely due to the dispersion and frequency-dependent attenuation, affects some pulses more dramatically than others. An unfortunate consequence is that it is not possible to give a precise starting time for the wavetrain.

**Sampling Parameters**

Based on analysis of several hundred signals, it was concluded that a complete "library" of garter spring signatures could be generated using the following parameters.

1. **Sample frequency:** A sample frequency of 500,000 points per second is recommended in order to study signals in a band of 30 - 60 kHz. This band should enable the desired resolution of ± 10 cm to be reached.
2. **Sample Time:** A sample time T of 2500 µs was found to be an optimum compromise between the goals of minimum computation time, and good spatial resolution between signals.
3. **Library Mesh:** A cylindrical mesh was generated consisting of signatures collected with an axial spacing of 2 cm, and circumferential spacing of 7 - 10°. Cross-correlation of a test signal was found to be high (≥ 0.70) only with library signatures from neighbouring mesh points (Figure 3). In general, cross correlation between any two signatures with a difference in D greater than 1 cm, or θ greater than 11°, was less than 0.40, for T ≥ 2500 µs. (Figure 4).

These results indicate that a library consisting of 1000 to 2000 signatures would be needed to describe all possible garter spring locations, with each signature containing about 1,250 data points. The desired resolution of ± 10 cm could be met (assuming other problems can be solved), although computation costs would be high.
Several potential problems remain to be explored. Among them are the following:

1. **Variability of a Signature**: Results of this work and data analyzed by Ontario Hydro suggest that a garter spring signature may be very sensitive to parameters that are unknown or not easily controlled, e.g., rolled joint variability, pressure tube wall thickness variability, type of endfitting, feeder tube geometry, pressure tube history & creep, bearing stress distribution, etc. This variability can be assessed only by taking measurements on a large number of fuel channels. If the variability is found to be too severe, it would be impossible to generate a signature library. Alternatively, the signature library might have to be prohibitively large, with attendant high computation costs.

2. **Background Noise**: The effects of flowing coolant and of fuel bundles inside the pressure tube remain to be investigated. This problem is important both from an analytical and practical standpoint. First, the weight of the fuel bundles and the presence of heavy water in contact with the inside walls of the pressure tube alter its resonant frequencies. Second, the bearing pads on the fuel assemblies, being in contact with the pressure tube, will respond to any excitation and thus become sources of "noise" in the acoustic emission signal.

3. **Source Generation**: A pinched garter spring may rub rather than impinge on a pressure tube. The variability of the signal under these conditions needs to be explored. The pinched springs couple the vibrations of the pressure tube to those of the calandria tube, thereby complicating the analysis of this phenomenon.

4. **Boundary Conditions**: The boundary conditions at the rolled joints are important from an analytical standpoint for modelling sound propagation in the pressure tube. An accurate model will permit optimum placement of the acoustic emission sensors; it may also indicate the preferred method of channel excitation.

**ANALYTICAL MODELLING**

**Shell Theory**

In order to better understand the propagation of sound in the fuel channel, work has been conducted on developing an analytical model.

For the purpose of analysis, the pressure tube can be considered to be a circular cylindrical shell of constant thickness and constant radius of curvature. The coordinate system for such a shell is shown in Figure 5, where \( a \) is the radius of the middle surface of the cylinder, \( h \) is the thickness of the cylinder wall, \( l \) is the length of the cylinder, \( x, \theta \) and \( z \) are the coordinates in the axial, tangential and radial direction respectively, with \( U, V \) and \( W \) representing the corresponding displacements.

The theories used in analyzing the static and dynamic behaviour of shells can be classified in many ways, but for our present purpose, three general types of theories must be distinguished. The first type, known as **Membrane Theory**, considers the shell to be an infinitely thin-walled membrane, which can only support extensional stresses and hence offers no resistance to bending. The second type, which encompasses a group of theories known as "thin shell theories", takes into account both extensional and bending stresses but, in a manner analogous to classical beam theory, neglects the effects of transverse shear deformation and rotatory inertia.

The third type, which will simply be referred to as "shell theory", takes these two effects into account. This results in a more accurate description of the shell behavior, at the expense of greater mathematical complexity, more numerical computation, and somewhat greater difficulty in interpreting the result. We sought to determine the conditions (in terms of frequency range and nodal patterns) under which the simplified "thin shell theories" will predict the vibration frequencies accurately, and those under which the more elaborate "shell theories" must be employed.

**Thin-Walled Shells**

Thin shell theories relate the displacements of any point in the shell wall to those of a corresponding point on the mid-surface of the shell in such a way that the only unknowns in the displacement equations of motion will be the three displacements \( u, v \) and \( w \) of the mid-surface. Thus, \( U, V \) and \( W \) are assumed to be of the form:

\[
\begin{align*}
U(x, \theta, z) & = u(x, \theta) + z \psi_z(x, \theta) \\
V(x, \theta, z) & = v(x, \theta) + z \psi_z(x, \theta) \\
W(x, \theta, z) & = w(x, \theta)
\end{align*}
\]  

(1)

where \( \psi_z \) and \( \psi_{\theta} \) represent the rotations of the normal about the \( \theta \) and \( z \) axes respectively. They can be related to \( v \) and \( w \) by virtue of the Love-Kirchhoff assumption that straight lines normal to the undeformed middle surface of the shell shall remain normal to the deformed middle surface, and suffer no extension. This yields:
\[ \psi'_z = -\frac{\partial w}{\partial x} \quad \psi'_\theta = \frac{v}{a} - \frac{1}{a} \frac{\partial w}{\partial \theta} \]  
(2)

The derivation of the thin shell theory then proceeds by establishing the strain-displacement, stress-strain and equilibrium equations of the shell; furthermore, in deriving the force equilibrium equations, the translatory inertia of the shell element along the \( x \), \( \theta \) and \( z \) axes is taken into account, while in the moment equilibrium equations the rotatory inertia of the element about the \( x \) and \( \theta \) axes is neglected. All stresses and strains are then eliminated from these equations to give the system of partial differential equations which describes the motion of thin shells. The equations can be expressed in matrix form as follows:

\[
\begin{bmatrix}
L_{11} & L_{12} & L_{13} \\
L_{21} & L_{22} & L_{23} \\
L_{31} & L_{32} & L_{33}
\end{bmatrix}
\begin{bmatrix}
u \\
v \\
w
\end{bmatrix} = 0
\]  
(3)

or, more compactly

\[
[L] \{u\}_n = 0,
\]  
(4)

where \([L]\) is the differential operator and \{\(u\)_n\} is the displacement vector. Various forms of thin shell theory exist, each of which yields a slightly different set of elements \(L_{ij}\). In this analysis, the theory of Flugge is used due to its generality and its ability to predict the behavior of thin shells accurately in most of their modes. The elements of the top row of the Flugge differential operator are:

\[ L_{11} = a^2 \frac{\partial^2}{\partial x^2} + \frac{(1-\mu)}{2} (1+k) \frac{\partial^2}{\partial \theta^2} - \rho \frac{(1-\mu) a^2}{E} \frac{\partial^2}{\partial t^2} \]
\[ L_{12} = a \frac{(1+\mu)}{2} \frac{\partial}{\partial x \partial \theta} \]
\[ L_{13} = \mu a \frac{\partial}{\partial x} - \frac{k^2}{12} \left[ a \frac{\partial^3}{\partial x^3} + \frac{(1-\mu)}{2a} \frac{\partial^3}{\partial x \partial \theta^2} \right] \]

where

\[ k = \frac{\mu}{a^2} \]

The reader can refer to Reference 8 (pp. 32-33) for the expressions for the other elements.

For our purposes, a solution of the Flugge equations is sought which satisfies the clamped-clamped boundary conditions. The reason for choosing clamped-clamped boundary conditions is that among the set of so-called "simple" boundary conditions applicable to this type of shell, the clamped-clamped conditions best approximate the rolled joint connections between the pressure tube and the heavy end-fittings.

Since Equations (1) are of order eight, we need four boundary conditions at each end of the shell in order to be able to determine the solution. The clamped-clamped boundary conditions can be expressed mathematically as:

\[ u = v = w = \psi'_z = 0 \quad \text{at} \quad x = 0, l \]  
(6)

A numerical method for the solution of Equations (4) subject to boundary conditions (6), which is based on the Rayleigh-Ritz principle, is given in reference (8). The trial functions selected for use with the Rayleigh-Ritz method are:

\[ u = A \phi(x) \cos \eta \theta \cos \omega t \]
\[ v = B \phi(x) \sin \eta \theta \cos \omega t \]
\[ w = C \phi(x) \cos \eta \theta \cos \omega t \]

where \( A \), \( B \) and \( C \) denote the undetermined amplitudes of the vibrations, \( \phi(x) \) represents the Euler-Bernoulli clamped-clamped beam mode shape, and the prime indicates differentiation with respect to the dimensionless beam span parameter \( \frac{I}{a} \). The frequency parameter \( \lambda_n \) is associated with the beam mode shape selected. The index "n" indicates the number of waves around the circumference of the shell; similarly, an index "m" is used to denote the number of axial half-waves.

Applications of the Rayleigh-Ritz method with the above trial functions will result in a matrix equation of the form:

\[
\begin{bmatrix}
a_{11} & a_{12} & a_{13} \\
a_{12} & a_{22} & a_{23} \\
a_{13} & a_{23} & a_{33}
\end{bmatrix}
\begin{bmatrix}A \\ B \\ C
\end{bmatrix} = \{0\}
\]  
(8)

For Equations (8) to admit non-trivial solutions, the determinant of the symmetric 3X3 matrix must be set equal to zero, yielding a cubic equation in the square of the angular frequency \( \omega^2 \). Thus, for each nodal pattern (i.e., fixing \( n \) and \( m \)) there are three distinct frequencies of vibration. Substitution of the values of \( \omega^2 \) into any two of Equations (8) will result in the determination of the amplitude ratios \( A/C \) and \( B/C \); these indicate the mode shape of vibration for each frequency.

The results of this procedure are shown in Figure (6) where the lowest vibration frequency for each nodal pattern is plotted against the non-dimensional parameter \( \frac{I}{ma} \). Both a low value for \( \frac{I}{ma} \) and a high value for \( n \) indicate that the wall of the cylinder has a high curvature during deformation; the effects of shear deformation and rotatory inertia are expected to be more pronounced in these cases.

**Effects of Shear Deformations and Rotary Inertia**

The effects of shear deformation and rotatory inertia can be accounted for by dropping the "normals remain normal" assumption, which is inherent to Equation (2). This results in \( \psi'_z \) and \( \psi'_\theta \) becoming two additional unknowns to be determined. Also, terms representing the rotatory inertia of the shell element will be added to the moment equilibrium equations. The number of equations will therefore increase to five. The new equations can be represented in matrix form as follows:

\[
[L'] \{u\}_n = \{0\}
\]  
(9)

where \([L']\) is the new 5X5 differential operator and...
\[
\{u_i\} = [u \ v \ w \ \psi_z \ \psi_y]^T \tag{10}
\]

As before, the elements of \([L']\) will vary with the shell theory used. In this analysis the equations of Mirkov and Herrmann have been used.

The new boundary conditions for use with Equation (9) will be:

\[
u = v = w = \psi_z = \psi_y = 0 \text{ at } x = 0, l \tag{11}
\]

The procedure for solving equation (9) subject to the boundary conditions (11) is similar to that already outlined for solving the simpler case. However, to avoid the evaluation of a 5X5 determinant and solution of a fifth order polynomial, an iterative algorithm has been adopted.

Approximating \(u, v\) and \(w\) by Equations (7), with \(A, B\) and \(C\) as previously determined, the Rayleigh-Ritz method is applied to Equation (9). This permits the last two rows of the matrix equation to be manipulated to give:

\[
\begin{bmatrix}
  b_{11} & b_{12} \\
  b_{21} & b_{22}
\end{bmatrix}
\begin{bmatrix}
  D/C \\
  F/C
\end{bmatrix} =
\begin{bmatrix}
  d_1 \\
  d_2
\end{bmatrix} \tag{12}
\]

where \(\psi_z\) and \(\psi_y\) have been assumed to be:

\[
\psi_z = D \phi'(x) \cos n \theta \cos \omega t \tag{13}
\]
\[
\psi_y = F \phi(x) \sin n \theta \cos \omega t
\]

The ratios \(D/C\) and \(F/C\) can therefore be determined from Equations (12). After some manipulation, it is possible to arrive at a modified form of Equations (2), namely,

\[
\psi_z = -\eta_1 \frac{\partial w}{\partial x}, \quad \psi_y = \eta_2 \left( \frac{v}{a} - \frac{1}{a} \frac{\partial w}{\partial \theta} \right) \tag{14}
\]

where

\[
\eta_1 = \frac{\lambda_m}{l}, \quad \eta_2 = \frac{a(F/C)}{(B/C) + n} \tag{15}
\]

Using the new expressions for \(\psi_z\) and \(\psi_y\), a new set of equations similar to Equations (8) are derived and numerically solved to give the first corrections to the frequencies of free vibration of the cylinder.

The results obtained after carrying out this iterative procedure are shown in Figure (6), superimposed on the previous results in which shear deformation and rotatory inertia had been neglected.

As expected, the results show that the effects of shear deformation and rotatory inertia are much more pronounced in mode shapes which involve many axial
and circumferential waves, due to the shorter wavelengths of these modes. Also, it is interesting to note that the relative error in frequency increases much more drastically with \( n \) than \( m \), this being a consequence of the geometry of the pressure-tube, i.e., a relatively small radius-to-thickness ratio and a very large length-to-radius ratio. Finally, the perturbation in resonant frequencies as a result of the improved theory is always negative. This is a consequence of the fact that by accounting for shear deformation and rotatory inertia, the effective stiffness of the cylinder is reduced.

**CONCLUSION**

Preliminary experiments have shown that locating the garter springs on a CANDU fuel channel may be feasible using an acoustic emission pattern recognition scheme. Analytical work is underway to better understand the characteristics of sound propagation along the fuel channel. Further experiments are planned to determine the effects of noise, coolant flow, and fuel bundles on the signal patterns.

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**REFERENCES**


5. Green, M.A., Ontario Hydro Electrical Research, Private Communication, April 1980.


ABSTRACT

Over the past eight years, a number of leaks have developed in the Biological Shield Cooling (BSC) System of the Pickering NGS A reactors. The highest leak rate exists in Unit 4. The failure mechanism is not known, but corrosion and/or weld failure are suspected. This paper summarizes the concerns associated with the leaks and possible solutions.

It should be noted that the BSC system is peculiar to Pickering A reactors only.

BIOLOGICAL SHIELD COOLING SYSTEM

The Biological Shield Cooling System is a demineralized light water system for cooling the calandria vault structure (see Figure 1). The rectangular calandria vault structure of heavy concrete encloses the calandria vessel and the dump tank. The term "biological shield" is used for this structure since one of its functions is to protect operating personnel from the nuclear radiation from the reactor. The heat input to the calandria vault structure is due to nuclear radiation heating and heat transfer from the calandria vessel and the PHT headers and feeders.

Most of the cooling water piping is embedded in the calandria vault concrete, within the walls, roof and floor. In addition to the embedded piping there are ring thermal shields enclosing the calandria subshells on both ends of the calandria (Figure 2). The thermal shields are composed of segmented hollow rings. Their purpose is to shield the vault concrete from nuclear heating.

Additional radiation shielding is provided by the end shield ring assemblies adjacent to the ring thermal shields. The end shield ring assemblies consist of large diameter steel rings, embedded in the calandria vault walls at both ends of the calandria. Cooling of the end shield ring assemblies is also provided by the BSC system.

Most of the biological shield cooling system is inaccessible, as it is either embedded in concrete or located in the highly radioactive calandria vault.

BACKGROUND/HISTORY OF LEAKS

The first leak within the calandria vault structure of PNGS A occurred in Unit 1 in August, 1978, after about seven years of operation. Since then, leaks have occurred in all Pickering A units except Unit 3.

The highest leak rate at the present time exists in Unit 4, where the leakage is originating from the ring thermal shields. These shields represent over 40 percent of the total heat sink of the system. Both ring thermal shields are leaking. The leak rate is within the capacity of the leak handling system and the leak to date has not affected the heat removal capability of the system.
Concerns Associated with the BSC Leaks

Leak Handling

The leakage collects in the calandria vault floor sump, which has a pipe connection to a leakage handling system located outside of the vault.

The leak rate is estimated from the head tank level change. The leaking line is identified by line-by-line isolation during reactor shutdown.

Current leak rates are approximately 5 kg/hr in Unit 1, 15 kg/hr in Unit 2 and about 400 kg/hr in Unit 4. Prior to an increase in the Unit 4 leak rate in March, 1985, the leakage from all three units had been collected in drums. The increase in the leak rate in Unit 4 imposed a significant workload and increased radiation exposure of personnel, therefore the leak handling of Unit 4 was modified.

Corrosion of Calandria Vault Components

Due to the radiolysis of vault air and the presence of leaking water, nitric acid (HNO₃) is formed in the vault. HNO₃ is corrosive to carbon steel (CS), especially at a pH of 5 or less. Measured pH of the leakage collected from the calandria vault in Unit 4 is about 5.5 while the pH of the cooling water is controlled between 9.5 to 10.5.

Chemical analyses of the Unit 4 calandria vault effluent show presence of iron signaling some corrosion of carbon steel in the calandria vault. Corrosion of stainless steel in the vault is very low, as indicated by traces of chromium in the effluent. A low content of Al, Ca, K, Mg, Ti and Si indicates negligible damage to the calandria vault concrete.

Temperatures and Stresses in the Calandria Vault Structure

A minimum coolant flow is necessary to maintain the specified upper limit on the concrete temperature. There is therefore a limit on the leak rate the structure can tolerate.

In addition to maximum concrete temperature, there is also concern about the temperature differentials within the structure. Temperature differentials result in differential thermal expansion which could lead to overstressing and cracking of both the concrete and metal components.

Resolution of Leaks/Plan of Action

Leak Sealing was attempted after the first leak occurred in Unit 1, using an automotive radiator sealant with a cellulose base. The leak was reduced for a short period of time (~40 days), subsequently returning to its original value. The line was later isolated.

A second attempt at leak sealing was made recently (April, 1986) to seal one of the ring thermal shields in Unit 4. A sealant proprietary to British National Nuclear Corporation containing bentonite clay and epoxy resin was used. While the sealing was successful on a mockup, the attempt failed in the field. Another attempt is planned for the next Unit 4 shutdown (spring, 1987).

Sealing of the small leaks in Units 1 and 2 is planned for the near future.

Another sealing compound is being developed for Ontario Hydro by Babcock & Wilcox Canada Ltd. Preliminary tests indicate a good possibility of sealing a leak several times larger than the present leaks in Units 1 and 2.

Isolation of Leaking Lines

Two BSC lines have so far been isolated, one in Unit 1 and one in Unit 4. Both of them are end shield ring cooling lines. Feasibility of isolation was checked by thermal analysis to determine the maximum concrete temperature with the cooling lines isolated. The lines were taken out of service by valving out the system, draining and drying.

The case of isolating the ring thermal shield lines is more complicated because it would mean elimination of a good portion of the heat sink of the BSC. By means of finite element thermal and stress analyses of the vault structure, a study was performed to show the feasibility of line isolation. First, the heat generation rates in the ring thermal shields were established by measurements at site and by new calculations. Both the measurements and the new calculations showed the heat generation rates to be only 50 percent of the rates very conservatively calculated during the original system design, about 20 years ago. The new calculated heat input to the calandria vault is shown in Figure 3.
(1) Normal water cooling in the entire BSC system;

(2) No cooling in the ring thermal shields (normal water cooling elsewhere);

(3) Air cooling in the ring thermal shields (normal water cooling elsewhere).

These analyses showed that isolation of the ring thermal shield cooling lines (case 2 above) is not feasible; the temperature and stress limits of the vault concrete would be exceeded. The minimum cooling water flow allowable in the ring thermal shields has been calculated to be about 10 percent of the normal water flow, compared with a current minimum through flow of about 97 percent.

Air cooling of the shields (case 3 above), which would eliminate the water leak, is feasible. Temperatures in the calandria vault structure would be slightly higher than those with the normal water cooling, but well within the allowable limit of 65°C (150°F). The concrete stresses will also be within the allowable limits.

The normal water cooling case has been validated by measurements at site, using the thermocouples installed in the ring thermal shield and in the calandria vault roof concrete. There is good agreement between the calculated and measured temperatures of the ring thermal shields. As for the calculated concrete temperatures, they are approximately 11°C (20°F) higher than the measured temperatures, due to the conservative approach used in the calculations. Figure 4 shows the calculated temperature distribution in four different planes of a typical calandria vault wall cross-section (east or west wall) outlined in Figure 5.

![Figure 5: E-W Model](image_url)

**Improved Leak Handling Capability**

The leak handling of Unit 4 has been modified to handle the increased volume of light water leaking from the BSC. The calandria vault sump effluent has been separated from all other sump effluents (boiler room and fuel transfer rooms) and provision has been made for pumping instead of drumming the effluent. The vault effluent is being pumped intermittently through charcoal filters and ion exchange columns to the Active Liquid Waste Management System (see Figure 6).

The leak handling system can handle a leak rate of 800 kg/hr, by continuous pumping. Further capacity increase could be achieved by modifications such as larger piping and additional storage tanks.

Circulation of the leakage back into the system is also possible.
The make-up would replenish the system to compensate for the air lost through the cracks. The purge system will limit the buildup of radioactivity.

**Leak Reduction by Manipulating the BSC System**

Reduction of the cooling water flow will not reduce the leak rate proportionally. The leak rate is a function of the system pressure at the leak location, which in turn is controlled by the relative elevation of the leak and the cooling water head tank. Because the head tank cannot be re-located to a lower elevation, the leak rate cannot be reduced appreciably.

If the leak occurs on the cooling water inlet side, reversing the flow direction through the ring thermal shield would reduce the leak, but only marginally.

**Reduction of Carbon Steel Corrosion in the Calandria Vault**

As a precaution against carbon steel corrosion due to low pH, the pH of the effluent is monitored and in case of a pH decrease, continuous injection of LIOH to the BSC system and pump-out of the calandria vault will be implemented.

**Air Cooling for the Ring Thermal Shields**

Air cooling of the ring thermal shields is feasible. The air cooling would replace water cooling in the ring thermal shield only; the rest of the BSC system would still be cooled by water. The cooling channels/pipes, although sized for water, are adequate for air cooling; no choking conditions occur.

A closed recirculating air cooling system with make-up and purge was selected, as it would not have an impact on existing air and ventilation systems of the station (see Figure 7).

**Stress analyses have been performed to confirm** that air escaping from the crack does not cause brittle crack propagation.

Other combinations of air and water cooling in the BSC system were considered, e.g., blowing air into the calandria vault interior with no cooling in the ring thermal shields but with water cooling in the rest of the BSC. They were found not to be feasible due to excessive air flow requirements and velocities.

Complete replacement of water cooling with air cooling is also not feasible. The air flow requirements and velocities would also be excessive.

**CONCLUSIONS**

While at the present leaks do not detrimentally affect reactor operation, solutions are being sought to prevent development of future problems such as corrosion, especially in the case of increased leak rates. Improved techniques for leak sealing and finalization of the design of the air cooling system for the ring thermal shields will be pursued in parallel.

In the meantime, visual inspections are planned to establish the failure mechanism, size, shape, location of leaking cracks and status of components which may be subjected to corrosion.

The thermal and stress models of the vault structure that have been developed will be a useful tool to promptly assess implications and remedial actions (line isolation) in case of further leaks.

Finally, possibilities for remote repair of leaking lines such as cutting, sleeving, welding, etc., using remotely operated tools/robots are being investigated.

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