TRANSIENT BEHAVIOUR OF HIGH BURNUP FUEL

Proceedings of the CSNI Specialist Meeting
Cadarache, France: 12-14 September 1995
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The primary objective of NEA is to promote co-operation among the governments of its participating countries in furthering the development of nuclear power as a safe, environmentally acceptable and economic energy source.

This is achieved by:

- encouraging harmonization of national regulatory policies and practices, with particular reference to the safety of nuclear installations, protection of man against ionizing radiation and preservation of the environment, radioactive waste management, and nuclear third party liability and insurance;
- assessing the contribution of nuclear power to the overall energy supply by keeping under review the technical and economic aspects of nuclear power growth and forecasting demand and supply for the different phases of the nuclear fuel cycle;
- developing exchanges of scientific and technical information particularly through participation in common services;
- setting up international research and development programmes and joint undertakings.

In these and related tasks, NEA works in close collaboration with the International Atomic Energy Agency in Vienna, with which it has concluded a Co-operation Agreement, as well as with other international organisations in the nuclear field.

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The NEA Committee on the Safety of Nuclear Installations (CSNI) is an international committee made up of scientists and engineers. It was set up in 1973 to develop and co-ordinate the activities of the Nuclear Energy Agency concerning the technical aspects of the design, construction and operation of nuclear installations insofar as they affect the safety of such installations. The Committee's purpose is to foster international co-operation in nuclear safety amongst the OECD Member countries.

CSNI constitutes a forum for the exchange of technical information and for collaboration between organisations which can contribute, from their respective backgrounds in research, development, engineering or regulation, to these activities and to the definition of its programme of work. It also reviews the state of knowledge on selected topics of nuclear safety technology and safety assessment, including operating experience. It initiates and conducts programmes identified by these reviews and assessments in order to overcome discrepancies, develop improvements and reach international consensus in different projects and International Standard Problems, and assists in the feedback of the results to participating organisations. Full use is also made of traditional methods of co-operation, such as information exchanges, establishment of working groups and organisation of conferences and specialist meetings.

The greater part of CSNI's current programme of work is concerned with safety technology of water reactors. The principal areas covered are operating experience and the human factor, reactor coolant system behaviour, various aspects of reactor component integrity, the phenomenology of radioactive releases in reactor accidents and their confinement, containment performance, risk assessment and severe accidents. The Committee also studies the safety of the fuel cycle, conducts periodic surveys of reactor safety research programmes and operates an international mechanism for exchanging reports on nuclear power plant incidents.

In implementing its programme, CSNI establishes co-operative mechanisms with NEA's Committee on Nuclear Regulatory Activities (CNRA), responsible for the activities of the Agency concerning the regulation, licensing and inspection of nuclear installations with regard to safety. It also co-operates with NEA's Committee on Radiation Protection and Public Health and NEA's Radioactive Waste Management Committee on matters of common interest.
FOREWORD

The CSNI Specialist Meeting on Transient Behaviour of High Burnup Fuel was held in Cadarache, France, from September 12th to 14th, 1995. It was hosted by the CEA Institut de Protection et de Sûreté Nucléaire (IPSN) at the Château located at the nuclear research centre of Cadarache. More than 125 experts from 15 OECD countries as well as experts from Russia and the IAEA attended the meeting. Thirty-two papers were presented in four sessions.

The purpose of the meeting was to bring together experts involved in the different activities related to high burnup fuel behaviour under transient conditions, and in particular during reactivity initiated accidents (RIA). The meeting focused on reactivity initiated accidents (RIA) because of the current interest in that subject and the significant amount of new technical information being generated. The meeting was structured around three main technical areas: integral experiments, separate effect tests and plant calculations, plus a background on the current regulatory status. Each of these areas corresponded to a separate session. The experts came from all involved parties, including research organisations, regulatory authorities, fuel designers and utilities. Information was openly shared and discussed on the integral experiments results, separate-effect tests findings and analytical assessments performed. Regulatory background and licensing implications were also included to provide the proper frame for the technical discussions.

These proceedings are published on the responsibility of the Secretary-General.
### TABLE OF CONTENTS

**Opening Remarks by Dr. Gianni FRESCURA**  
Head, Nuclear Safety Division, OECD/NEA.  
13

#### Session I: REGULATORY BACKGROUND

Co-Chair: R. MEYER and C. MAEDER

Summary of the Session  
15

- "USNRC Review of High Burnup Fuel Regulatory Requirements"  
  L.E. PHILLIPS  
  17

- "High Burn-up Fuel: French Safety Authority Position"  
  D. LAGARDE, V. JACQ, J. LEWI, M. CHAMP  
  33

- "Regulatory Status on Transient Behaviour of High Burnup Fuel and Related Research Activities in Japan"  
  T. SATO, T. FUJISHIRO  
  39

- "German Licensing Approach and Consequences for High Burnup Fuel"  
  S. LANGENBUCH, Ch. FABER  
  45

#### Session II: INTEGRAL TESTS AND ANALYSIS

Co-Chair: T. FUJISHIRO and M. HAESSLER

Summary of the Session  
55

- "Behaviour of High Burnup PWR Fuel Under a Simulated RIA Condition in the NSRR"  
  T. FUKETA, Y. MORI, H. SASAJIMA, K. ISHIJIMA, T. FUJISHIRO  
  59
« Postulated Mechanisms on the Failure of 50 MWd/kgU PWR Fuel in the NSRR Experiment and the Related Research Programs in JAERI »
K. ISHIJIMA, Y. MORI, T. FUKETA, H. SASAJIMA .......................................................... 87

« The Experimental Test Programme for the Study of High-Burnup PWR Rods Under RIA Conditions in the CABRI Core »
M.C. ANSELMET-VITIELLO, F. ARREGHINI, M. HAESSLER ........................................... 107

« Cladding and Fuel Modifications of a 60 GWj/m Irradiated Rod During a Power Transient Performed in the CABRI Reactor »
P. MENUT, D. LESPIAUX, M. TROTABAS .......................................................................... 127

« The Behaviour of Irradiated Fuel Under RIA Transients: Interpretation of the CABRI Experiments »
J. PAPIN, H. RIGAT, J.P. BRETON .................................................................................. 137

« Development and Performance of a Research Programme for the Analysis of High Burn-up Fuel Rods Behaviour under RIA Conditions in the IGR Pulse Reactor »
V. ASMOLOV, L. YEGOROVA ......................................................................................... 155

« Primary Factors Causing the Failure of High-Burnup LWR Fuel Rods During Simulated Reactivity Initiated Accidents »
R.K. McCARDELL, R.O. MEYER .................................................................................... 167

« Unexpected Transient on an Experimental Instrumented Fuel Rod in BR2 »
S. BODARD, B. COUPE, V. SOBOLEV, M. LIPPENS ......................................................... 185

« STUDSVIK's Experience Related to LWR Fuel Behaviour at High Burnup »
H. MOGARD, M. GROUNES .............................................................................................. 213

Session III: PLANT CALCULATIONS
Co-Chair: S. LANGENBUCH and K. VALTONEN

Summary of the Session .................................................................................. 243

« A Best-Estimate Assessment of Rod Ejection Fuel Duty in PWRs »

« Study of the Rod Ejection Transient on a PWR Related to High Burnup Fuel Rupture Risk »
S. STELLETTA, M. MOREAU .............................................................................................. 251

« On the Role of Burnup Effects of Fuel Properties in RIA Analysis »
R. KYRKI-RAJAMAKI ..................................................................................................... 269
« Analysis of the Fuel Behaviour under Rod Ejection Accident in the PWR »

« Realistic Scoping Study of Reactivity Insertion Accidents for a Typical PWR and BWR Core »
L.J. AGEE, A.F. DIAS, L.D. EISENHART, R.E. ENGEL

« Methodology and Results of RIA Studies at Siemens »
D. BENDER, H. BAUER, F. WEHLE, D. KREUTER, H. FINNEMANN, J.L. MARYOTT, R.A. COPELAND

« Analyzing the BWR Rod Drop Accident in High-Burnup Cores »
D.J. DIAMOND, L. NEYMOTIN, P. KOHUT

« Investigations Related to Increased Safety Requirements for Reactivity Initiated Accidents »
F. HOLZGREWE, J.M. KALLFELZ, M.A. ZIMMERMANN, C. MAEDER, U. SCHMOCKER

« Analyses of Rod Drop Accidents Using a Three-Dimensional Transient Code for Reactivity-Initiated Events of Boiling Water Reactors »
T. OTA, T. ANEGAWA, A. OMOTO, T. OTA, M. NAGANO, S. IZUTSU

« Realistic Evaluation of Reactivity Insertion Accidents in Boiling Water Reactors »

Session IV : SEPARATE-EFFECT TEST AND ANALYSIS
Co-Chair : V. LANGMANN and A. DELBRASSINE

Summary of the Session

« High Burnup Phenomena - Results from Experiments in the Halden Reactor »
W. WIESENACK

« Tensile Properties of Irradiated Zircaloy-4 Cladding Submitted to Fast Transient Loading »
M. BALOURDET, C. BERNAUDAT

« Influence of Locally Concentrated Hybrides on Ductility of Zircaloy-4 »
F. NAGASE, K. ISHIJIMA, T. FURUTA
CSNI Specialist Meeting on 
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Cadarache, France, 12th-14th September 1995

Opening Remarks by Dr. Gianni FRESCURA
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OPENING REMARKS (G.M. FRESCURA, Head, Nuclear Safety Division, OECD/NEA)

It is a great pleasure for me to bring the welcome of the OECD/NEA and its Committee on the Safety of Nuclear Installations to this CSNI Specialist Meeting on Transient Behaviour of High Burnup Fuel. I wish to thank the ISPN and the ISPN/DRS for hosting this meeting and for providing the local organisation.

If the performance of the Programme Committee - which is chaired by Dr. Franz Schmitz - can be measured by the number of participants to the meeting and their level of expertise, then I would say that they have indeed done very well.

I am delighted to see that the participation includes - in addition to researchers - also a good representation from regulators, fuel designers and manufacturers, as well as the utilities. The issues of high burnup fuel, like most issues of nuclear safety, require for their resolution close co-operation between researchers, regulators and industry.

I believe that most of the experts in this technical area from the OECD Member countries are here today. In addition, we have a number of colleagues from Russia who are going to share with us their valuable experience in this area.

Motivation

The general motivation behind this Specialist meeting is quite straightforward. In the case of LWRs, one way to improve the economics is to accommodate longer fuel cycles. Recently, the discharge burnup of many LWRs has increased from 40,000 to 60,000 MWd/tU. While there is enough data and operating experience to show that the fuel performance under normal operating conditions is satisfactory at these burnups, the transient behaviour of the fuel requires additional consideration. Of particular concern, of course, is the fuel behaviour during the rapid power increases resulting from some postulated reactivity initiated accidents (RIAs). I should add that the subject of fuel behaviour during rapid power transients is also of continuing high interest to other reactor designs such as CANDUs.

As we know, recent tests performed in France and Japan seem to indicate that the fuel failure limits used in the past, which were largely based on fresh fuel tests, may be lower than expected. I think it is generally accepted, for the reactors operated in the OECD Member countries, that the potential for reactivity initiated accidents has been adequately recognised in the design phase and dealt with by a combination of inherent and engineered features. However, destructive RIAs have taken place in the past in research and prototype reactors - NRX in Canada and EBR and SL-1 in the USA are some examples. More recently, the Chernobyl accident provided a tragic reminder of the potential consequences of RIAs. It is, therefore, imperative that RIAs be prevented with a high level of confidence and that the fuel limits for RIAs be well known and that conservative safety margins be maintained for fuel operating at high burnup.

The CSNI discussed this situation last November and, acting on a proposal from Principal Working Group-2 (the group that deals with coolant system behaviour), decided to establish an ad-hoc international group of experts to compile a status report and to formulate, if necessary, proposals for future international activities. This Specialist meeting is a key step in that process.
What are the issues and what can be expected out of this meeting?

I think the meeting should allow a thorough international review of the large amount of experimental information which is being generated not only in France and Japan but also in Russia and other countries. This review should be complemented by a review of the relevant modelling activities. The meeting should allow an in-depth discussion of the range of conditions expected in power reactors during postulated RIAs to ensure that the relevant conditions and range of parameters are properly bounded by the research results.

It is also important to review the regulatory positions and ensure that the relevant technical information is properly reflected into the regulatory requirements.

On the more detailed technical side, I think that the meeting should debate and try to provide a collective answer to the following questions:

1. Have all the phenomena relevant to fuel cladding failure for high burnups been identified? Are they sufficiently understood?

2. Have the findings from separate effect tests been effectively applied to integral experiments?

3. Are the integral tests representative of the conditions that can be expected in the nuclear power reactors during normal operating conditions and accident situations?

4. Are the phenomena of interest adequately modelled? Is additional effort on validation and evaluation of uncertainty required?

5. Are there enough experimental data to allow designers to confidently propose fuel design and operating limits and for regulatory bodies to review and approve these limits?

I believe that based on the answers to these questions the meeting should then consider whether there is a need to carry out additional work. If this is felt to be the case, the NEA through the CSNI and PWG-2 would certainly continue to provide support for your activities by promoting exchange of information or any other international undertaking that may be required.

Again, welcome to what promises to be a very useful and productive meeting.

G.M. Frescura
9 September, 1995
SUMMARY OF SESSION 1: REGULATORY BACKGROUND

The introductory session provided background information and established the safety relevance of the research papers presented in the meeting. The meeting focused on the behaviour of high burnup fuel during reactivity initiated accidents (RIAs). In the session, burnup levels licensed and achieved to date were described. Expected burnup extensions, current fuel damage criteria and regulatory actions taken in response to recent high burnup test data were discussed by members of regulatory authorities from USA, France, Japan and Germany. Similar information for Switzerland was presented in a paper in Session III and is included in this summary.

Licensed burnup limits, burnup levels achieved to date and expected burnup extensions

The following licensed burnup limits have been reported:

- a maximum rod-average burnup of 60 GWd/t in USA
- a generic limit for the assembly average burnup of 47 GWd/t in France
- generic limits for the assembly average burnup of 48 GWd/t for PWRs and of 50 GWd/t for BWRs in Japan
- Examples of maximum assembly average burnups are 65 GWd/t for PWRs and 53 GWd/t for BWRs in Germany.
- assembly average burnups of 48 to 60 GWd/t for different reactors in Switzerland.

In the US, assembly average burnup levels up to 52 GWd/t have been achieved and requests for a peak rod average burnup up to about 75 GWd/t have been proposed to the USNRC. In France, Japan and Germany, the utilities aim to increase the maximum assembly average burnup from 42-50 GWd/t to 52-55 GWd/t with a corresponding peak pellet average burnup of about 65 GWd/t in the near future. In Switzerland, peak pellet average burnups up to 66 GWd/t have been attained.

Current fuel damage criteria

There is general agreement that a control rod ejection accident in a PWR or a control rod drop accident in a BWR must not result in a loss of core coolability or mechanical damage to the primary system. Based on earlier experiments, it was assumed that this design goal can be satisfied if fuel fragmentation and dispersal is prevented and corresponding radially-averaged peak pellet enthalpy limits of 280 Cal/g in USA and Switzerland, 230 Cal/g in Japan and 200 Cal/g in France have been prescribed. As cladding failure threshold for burnup fuel radially-averaged peak pellet enthalpy limits of 170 Cal/g (in USA, Germany and Switzerland) or of 85 Cal/g (in Japan) have been defined. In Germany, a RIA must not cause any damage to the reactor core and is classified as an event without radiological impact and so cladding failures must be avoided.
Regulatory actions taken in response to high burnup test data

In response to the recent high burnup RIA test data, the following regulatory actions have been taken in some countries:

- Decisions on requests for extension of burnup limits beyond the maximum value previously approved were suspended until sufficient supporting transient performance data were available.

- The industry was asked to evaluate the significance of lower enthalpy thresholds for cladding failure and fuel dispersal on the safety of the plant and the radiological consequences. It was found that, if the present burnup limits were respected, no immediate threat to the safe operation of LWRs was posed.

- In several countries, a detailed evaluation of the transient test data for high burnup fuel was initiated and a co-ordinated effort between the industry, international experimenters and regulatory representatives is being pursued to develop appropriate fuel damage criteria for use in safety analyses.

- A new provisional licensing criterion for fuel cladding failure that is burnup dependent is being used in Switzerland:

  \[ \Delta E \text{ (Cal/g)} = 125 - 1.6B \text{ (GWd/t)} \]

In summary:

(a) Different licensing procedures are used in different countries.
(b) Several presenters suggested that additional technical information is needed before regulatory positions are modified.
USNRC REVIEW OF HIGH BURNUP FUEL REGULATORY REQUIREMENTS

by Laurence E. Phillips
Office of Nuclear Reactor Regulation
United States Nuclear Regulatory Commission

Specialist Meeting on Transient Behaviour of High Burnup Fuel
Cadarache, France, September 12, 1995
SESSION I: REGULATORY BACKGROUND

INTRODUCTION

In the mid-1980s, the staff of the U.S. Nuclear Regulatory Commission (NRC) approved for licensing applications the fuel vendor computer codes and models used to predict high burnup fuel performance behavior in operating light water reactors (LWRs) in the United States. At that time, the fuel batch average discharge exposures for boiling water reactors (BWRs) and pressurized water reactors (PWRs) ranged from 28 to 33 giga-watt days per tonne (GWd/t). The industry sought and obtained approval for application of its predictive models to burnup levels that would support extending the batch average discharge exposure range to 40 to 50 GWd/t. As the fuel vendors gained additional experience on the effects of extended burnup on fuel pellet and cladding characteristics by means of experimental programs, lead test fuel assemblies, and postirradiation examinations of extended burnup fuel rods, the data acquired were used to benchmark and modify, when necessary, fuel performance analysis codes and methods that were validated up to a peak rod average exposure level of about 60 GWd/t. Concurrently, fuel vendors sought and obtained approval for higher burnup fuel designs in order to accommodate core designs based on longer fuel cycles (18 and 24 months), which provide better operating economy. Additionally, the higher burnup fuel usage alleviates the rate of fuel depletion providing some relief with regard to the demand for spent fuel storage capacity at operating plants.

Ten years later (in the mid-1990s), the industry has acquired substantial experience with fuel burnup levels corresponding to
the 60 GWd/t peak rod average exposure limit discussed above. The USNRC is receiving requests from licensees and fuel designers to further extend the design peak rod average burnup limit to about 75 GWd/t on the one hand, while the safety of operation at current burnup limits is being questioned on the other hand, because of experimental evidence that the current NRC licensing acceptance criteria for fuel behavior during reactivity insertion transients may be non-conservative (R-1). In addition, available data indicate that some of the fuel pellet and cladding behavior models may require modification to correctly predict the burnup-dependent behavior beyond 60 GWd/t.

The objective of this paper is to examine the NRC licensing requirements for approval of new fuel designs and to discuss the safety assessment of operating reactors subjected to design-basis reactivity insertion transients. The potential impact of new experimental information regarding fuel damage thresholds for high burnup fuel is evaluated. Current burnup distributions in operating reactors in the United States are presented, and the NRC concerns and licensing position regarding high burnup fuel in the near term and plans for continuing evaluation are discussed (R-2). The outlook for design limits and burnup limitations for future reactor cores and industry actions needed to improve and justify fuel performance at higher burnup levels based on supporting experimental data and operating experience conclude the presentation.

NRC REQUIREMENTS FOR CORE RELOADS USING NEW FUEL DESIGNS

The requirement for NRC review and approval of the use of a new fuel design in a licensed facility depends on whether a technical specification change is needed or whether the licensee determines that inclusion of the fuel in a reactor will result in an unreviewed safety question as defined in Section 50.59 of Title 10 of the Code of Federal Regulations (10 CFR). In practice,
fuel vendors submit licensing topical reports (LTRs) describing in detail new fuel product designs and the results of design analyses and testing that demonstrate that the design conforms to the acceptance criteria in Section 4.2 of the NRC Standard Review Plan (SRP), NUREG-0800 (R-3). The fuel design safety analyses are performed using code models and methods that the NRC has also reviewed and approved, usually on the basis of other LTRs that have previously been submitted to the NRC such as the LTRs (R-4 through R-8) on high burnup performance behavior. Safety analyses of the reload core design are also performed to assure that the new fuel and core will satisfy all transient response design criteria for design-basis transients initiated during normal operation within the required constraints of the plant technical specifications. Technical specification changes are usually necessitated when design changes affect the thermal hydraulic characteristics of a fuel assembly in such a way that thermal limits must be revised or the allowable enrichment must be increased.

The SRP provides for the application of approved dryout correlations, i.e., departure from nucleate boiling ratio (DNBR) and critical power ratio (CPR), to define the thermal limits that, if exceeded, are assumed to result in fuel failure in the core safety evaluation. Although overheating of the cladding as a result of critical heat flux (dryout) does not equate directly to fuel failure, it is a condition that can be monitored using appropriate correlation to core operating parameters such as power and flow, and reactor scram can be initiated to preserve thermal margin limits in response to anticipated operating transients. For design-basis accidents, including reactivity insertion transients, fuel failures are permitted to the extent that acceptance criteria based on radiological dose calculations can be satisfied. For most of the design-basis accidents, the core design safety evaluation uses exceedence of the thermal limits as the measure of fuel damage. However, the SRP does
provide for the use of a radially averaged peak fuel rod enthalpy criterion of 170 calories/gram (cal/g) as the measure of fuel rod failure (by dryout) in the safety analyses for the BWR control rod drop accident initiated at zero or low power. In addition, a fuel energy deposition limit of less than 280 cal/g for the peak pellet in the core is the acceptance criterion for both the BWR rod drop and the PWR rod ejection safety evaluations. The purpose of this limit is to prevent fuel fragmentation and dispersal as indicated by the early transient fuel testing at the Idaho National Engineering Laboratory in the special power excursion reactor test (SPERT) and power burst facility (PBF) test reactors. Dispersion of high enthalpy molten fuel may result in rapid transfer of stored energy from the fuel to the coolant, with the possibility of creating a steam pressure pulse that could cause mechanical deformation adverse to continued core coolability.

In recent years, fuel vendors have submitted for NRC review "fuel design acceptance criteria" that significantly expand the scope of fuel design changes that can be made to an NRC approved fuel design without further NRC review. In this approach, the fuel vendor is required to perform and document a safety evaluation, which is subject to NRC audit, showing that all of the design acceptance criteria are satisfied.

**Licensing Considerations for Increase in Burnup Limits**

The industry trend toward longer fuel cycles requiring higher initial enrichments to maintain the core reactivity at extended burnup levels has focused attention on burnup-related fuel behavior characteristics. Loss of cladding ductility and fatigue strength because of accelerated oxidation of Zircaloy-4 and associated hydrogen absorption has been a concern and an issue in the licensing approval of extended burnup fuel designs. The low enthalpy level brittle failures observed in recent reactivity
insertion experiments with high burnup fuel (R-9) have magnified this issue. Other fuel behavior characteristics that have been noted to change in the 50 to 60 Gwd/t exposure range include fission gas release as a function of temperature, fuel thermal conductivity, gap conductance, fuel swelling, and fuel rim structure due to plutonium buildup (R-9). Within the burnup range currently approved, specific fuel design limits such as surface oxidation thickness and surface temperature limits have been developed by fuel vendors to limit the effect of corrosion on cladding material properties and ensure conformance to related strain acceptance criteria of the SRP.

Such limits also provide fuel designer flexibility to meet the design requirements by improvements in cladding material to reduce oxidation and loss of ductility at high burnup levels. Material design changes have ranged from major material changes such as Westinghouse Zirlo cladding material to changes in Zircaloy specifications. Reduction of Zircaloy-4 tin content or changes in heat treatment and grain size control are examples of changes used to achieve better corrosion resistance and material behavior after high irradiation exposure. Material properties at design exposure limits must be evaluated to demonstrate the material's capability to meet the strain acceptance criteria.

SAFETY ASSESSMENT OF RECENT DATA ON TRANSIENT BEHAVIOR OF HIGH BURNUP FUEL

An NRC safety assessment of high burnup fuel performance behavior was initiated in response to recent experimental data (R-10) suggesting a decline of the fuel rod enthalpy thresholds for cladding failure and for fuel fragmentation and dispersal as the fuel irradiation exposure level increases. The safety significance of the potential degradation of fuel behavior with regard to the consequences of design-basis transients and accidents for high burnup cores was assessed by the NRC staff and
the reactor industry while the experimental assessment to evaluate appropriate fuel damage limits has continued.

Early studies by the NRC staff included the review of final safety analysis report (FSAR) Chapter 15 accident analysis transients for a typical BWR and a typical PWR to identify the transients likely to result in high burnup (60 GWd/t) fuel approaching or exceeding 15 cal/g radially averaged energy deposition, a bounding value based on the available data. For the study, it was assumed that the average power traces presented in the FSAR analysis results are representative of the peak power transients experienced in high burnup (60 GWd/t) fuel. This assumption is valid for cores operating with power and burnup distribution patterns that result in peak power that does not exceed the core average power in the high burnup fuel rods. Later studies of core-loading patterns in operating reactors revealed that this assumption was slightly nonconservative because of checkerboard loading patterns mixing the depleted fuel with adjacent fresh fuel in higher-than-average power regions of many cores. Nevertheless, the study correctly showed that only a few of the FSAR transients were likely to result in some fuel approaching or exceeding the bounding transient energy deposition value of 15 cal/g. The following transients were identified:

- BWR rod drop
- PWR rod ejection
- BWR flow controller failure with recirculation flow increase
- BWR power oscillations (density wave instability)

It was concluded that the probability is very low for occurrence of reactivity transients that might result in some increase in fuel damage fraction determined by previous safety analyses. The PWR rod ejection and BWR rod drop transients were selected for more detailed analytical studies to evaluate the effect of lower enthalpy fuel failure thresholds on predicted fuel failure.
estimates for these transients and the corresponding radiological consequences.

REGULATORY ACTIONS IN RESPONSE TO HIGH BURNUP FUEL TRANSIENT RESPONSE DATA

On the basis of the safety assessment described above, the NRC staff concluded that no immediate threat to the safe operation of LWRs was posed by even the most pessimistic interpretation of the new high burnup fuel experiments. Only low-probability events appeared to be affected, and the NRC staff estimated that the fraction of high burnup fuel currently in most operating reactors was too small to pose a significant risk to the public health and safety even if all of that fuel were to fail during these events. Therefore, an action plan was developed to provide for more detailed study of the high burnup fuel performance behavior and the need for further regulatory action while operation of LWRs, in accordance with existing license requirements, is continuing. However, the NRC staff did notify the industry that it would not approve requests for extension of burnup limits beyond the maximum value previously approved for each fuel vendor and each fuel type until supporting transient performance data were available.

The major components of the NRC action plan are (1) evaluation of recent and planned transient test data for high burnup fuel; (2) detailed study of the most limiting transients to assess the effect of degraded high burnup limits on the accident consequences; (3) coordinated efforts with the industry to develop revised fuel damage criteria, if appropriate; and (4) coordinated efforts with international experimenters and regulatory representatives to obtain all necessary data to develop appropriate regulatory positions with respect to fuel burnup limits and conservative fuel design acceptance criteria for use in licensing safety analyses.
CURRENT STATUS OF NRC ACTIONS

Several meetings between the NRC and the nuclear industry have been held to discuss high burnup fuel issues and actions needed to resolve them. The Nuclear Energy Institute (NEI) has coordinated an industry generic safety assessment to determine the potential safety implication of lower fuel damage thresholds suggested by the preliminary experimental data for high burnup fuel. Results of the industry study and available data on core burnup and power distributions for 17 operating reactors were provided to the staff by a transmittal dated December 28, 1994 (R-11), and similar data for an additional 87 reactors were provided with a second transmittal dated February 28, 1995 (R-12).

The generic safety assessment by the industry concluded that lower fuel failure thresholds for the high burnup fuel would not have a significant impact on public health and safety because PWR rod ejection and BWR rod drop events are of low probability (10E-4 to 10E-6 per year assumed for PWR, zero actual events in 2400 reactor-years) and the radiological consequences of high burnup fuel failures would be limited. The rod ejection scoping studies indicated that ejection of a bounding case high worth rod (-1% delta-k/k (dk/k)) would result in less than 20 percent of the fuel rods achieving a peak fuel enthalpy in excess of 30 cal/g. A more typical case rod worth (-0.6% dk/k) resulted in about 7 percent of the core achieving similar enthalpies. Similar conclusions were reached for the BWR rod drop accident. Studies estimating the probability of a rod drop accident with fuel enthalpy exceeding 280 cal/g at less than 10E-12 per year were cited. Results of rod drop studies with a high worth rod (1.07% dk/k) showed peak fuel enthalpy of 117 cal/g for fresh fuel and estimated values of only 30 cal/g for fuel with bundle exposure of 66 GWD/t. However, details of the analyses to support these results were not presented. Many of the arguments
presented to support the safety conclusions were very qualitative in nature, and some of the analysis assumptions may not be acceptable to show compliance with licensing limits. Rod drop analyses performed for the NRC by Brookhaven National Laboratory (BNL) indicate fuel enthalpy transients in high burnup fuel can be a factor of 2 to 4 greater than indicated by the industry evaluation. For case studies, BNL used checkerboard-type fuel loading patterns, based on the NEI submittal, for the high burnup fuel but did not adhere to realistic rod control patterns.

The NRC has reviewed the core burnup data provided by the industry and has estimated the current burnup distributions in operating reactors. Figure 1 illustrates the estimated overall fuel assembly average burnup distribution for all fuel at the end of cycle for current fuel cycles in operating reactors in the United States. Figure 2 illustrates the same data by reactor type (different fuel vendors are involved for many reactors) and includes the estimated bounding burnup distributions for each reactor type. Note that the peak rod average burnup may be 5 to 10 percent greater than the illustrated fuel bundle average, and the peak pellet average burnup may be 10 to 15 percent greater than the fuel bundle average.

In addition to the NEI submittals, the NRC requested that U.S. fuel suppliers reexamine NRC conclusions on the acceptability of approved LTRs regarding specific fuel designs, fuel design acceptance criteria, and fuel behavior methodologies for application in the high burnup range of concern on the basis of the new data presented at the 22nd Water Reactor Safety Meeting. They were requested to advise NRC of the previously approved LTRs that may be affected, and to revise the LTRs or submit justification for the continued applicability of the LTR approvals in light of the new experimental data on transient fuel behavior. Fuel vendor responses have included detailed rod ejection analyses to evaluate more precisely the bounding impact.
of reduced fuel damage thresholds, and fuel vendors that have not provided similar analyses (including BWR rod drop) have indicated that they will do so.

The Westinghouse submittal (R-13) gave additional details regarding industry rod ejection accident scoping studies and the results of several hundred rod ejection case studies that supported information presented in the NEI submittal (R-11). Westinghouse also concluded that the rod ejection event results in a very localized increase in peaking factors in neighboring assemblies that appears to be independent of the fuel burnup distribution but is related to the reactivity worth of the ejected rod.

Westinghouse also evaluated the effect of additional fuel failures on the radiological consequences of the rod ejection accident. It concluded that additional failure of all of the high burnup rods in the core exceeding 30 cal/g would not result in activity release exceeding the radiological acceptance criterion (25 percent of the limits stipulated in 10 CFR Part 100) for the rod ejection accident. The evaluation assumed that the primary release path is from postulated containment leakage but took credit for (removed) several significant conservatisms in the design-basis analysis.

CONCLUSIONS

Preliminary safety assessments of new fuel behavior data by both the NRC and the industry indicated that existing reactor cores can continue to operate to approved design burnup levels without challenging licensing acceptance criteria for design-basis accidents. The margin available to licensing limits for radiological consequences appears to be sufficient to permit continued use of a small number of lead test assemblies to obtain performance data for higher burnup levels. More detailed studies
by the industry and NRC review are continuing and more work is needed to determine real transient fuel damage thresholds as a function of burnup. Details of transient experiments including the typicality and integrity of test fuel rods and the root cause and conditions of failure must be studied exhaustively to ensure appropriate interpretation of the data for evaluating fuel damage criteria. Experimental attention is needed to evaluate low enthalpy level transient failure behavior of high burnup fuel to ensure that the energy transfer during fragmentation and dispersal of solid fuel cannot result in loss of core coolability. Although the stored energy level of the solid fuel is much lower than that of the high enthalpy (molten and vaporized) fuel, the efficiency of energy transfer could be much higher. The SPERT data indicate that much less than 1 percent of the thermal energy transferred to the coolant from high enthalpy fuel (> 280 cal/g) is converted to mechanical energy loads.

Before continued extension of fuel cycle lengths and associated burnup levels, data are needed to better establish the relationship between measurable fuel performance parameters, such as oxide thickness, and related impact on the fuel cladding mechanical properties that affect the transient fuel damage threshold. Appropriate design acceptance criteria are needed to recognize and encourage fuel design-specific improvement in fuel cladding materials to achieve better high burnup performance. However, assessment of high burnup fuel design improvements based on capability for conformance to NRC radiological dose limits for accidents does not address all issues associated with the high burnup fuel usage. For example, there have been several recent instances of secondary hydriding fuel rod failures resulting in substantial release of fuel materials into the reactor or spent fuel storage pools (R-14). The characteristics of the secondary hydriding failures appear to be more severe than previously observed. Suspect contributors to such failures include: 1) cladding material design changes for improved high burnup
performance, and 2) extended residence of fuel in the operating reactor environment. Fuel performance during normal operation and the condition of fuel discharged for handling and storage in the spent fuel pool are important considerations. In-depth root cause analyses of fuel failures involving excessive nuclear material release would aid in the development of design or operational corrective measures. It is incumbent on the industry to reevaluate the safety issues and to develop appropriate design and acceptance criteria to address these issues.
REFERENCES

1. U.S. NUCLEAR REGULATORY COMMISSION; NRC Information Notice 94-64, Supplement 1: "Reactivity Insertion Transient and Accident Limits for High Burnup Fuel," April 6, 1995


7. CEN-386-P-A, "Verification of the Acceptability of a 1-Pin Burnup Limit of 60 MWD/Kg for CE 16x16 PWR Fuel," Combustion Engineering, June 1992


11. Letter from A. Marion (NEI), to R. Jones (NRC), "Nuclear Energy Institute Response to NRC Staff's Request for Information on Reactivity Insertion Accidents," December 28, 1994


Figure 1 Overall Fuel Assembly Average Burnup Distribution
Vendor Burnup Patterns
End of Cycle

Figure 2 Fuel Assembly Burnup Distribution by Reactor Type

Peak fuel pin average BU may be 5 to 10% greater, and peak fuel pellet 10 to 15% greater.
I - INTRODUCTION

Since 1989, the French safety authority (DSIN) has authorised Electricité de France (EDF), the French electric utility, to operate its 900 MWe plants within the burn-up limit of 47000 MW.day/ton U in fuel assembly average.

In 1990, necessary conditions for a generic authorisation allowing EDF to operate its plants beyond this limit were defined. In particular, EDF had to assess by an experimental programme, the behaviour of high burn-up fuel in design basis accident situations such as LOCA (loss of coolant accident) and RIA (reactivity insertion accident). It had to demonstrate that the present safety criteria were valid, otherwise new criteria would have to be determined and introduced in the safety studies.

For RIA, one important criterion to be met concerns the maximum mean fuel rod enthalpy ($H_{\text{max}}$), determined by neutronic calculations, which is liable to be reached in case of control rod ejection in start-up conditions:

$H_{\text{max}} < 225 \text{ cal/g for new fuel and}$

$H_{\text{max}} < 200 \text{ cal/g for irradiated fuel.}$

This document displays the present position of the French safety authority on high burn-up fuel issues as defined at the beginning of 1995 on the basis of the results of available tests.
II - TESTS RESULTS

The values previously adopted for the reactivity insertion accidents (RIA) criteria are based on the results of several hundred reactivity injection experiments carried out in the United States at the end of the 1970s on the SPERT installation (Special Power Excursion Reactor Test).

Only 10 tests were carried out with irradiated fuel, among which 2 with fuel of around 33 000 MW.day/ton U.

More recently, the results of tests performed in Japan in the NSRR (Nuclear Safety Research Reactor) have enriched this data base. The initial tests carried out on fuel pins with a burn-up lower than 35000 MW day/ton U have confirmed that the safety criteria are well grounded.

For fuel burn-up exceeding 45000 MW day/ton U, a very porous layer develops at the surface of the fuel pellet which has a high plutonium and gaseous fission products content. Moreover, the kinetics of clad oxidation and hydriding are greater with increased irradiation. These are phenomena which lead to clad embrittlement. These factors are conducive to clad rupture and even fuel dispersion for enthalpies below 200 g/cal, when the fuel burn-up increases.

The necessity to assess the behaviour of fuel elements at high burn-up has motivated an experimental program, CABRI, at Cadarache using samples with a burn-up exceeding 30,000 MW.day/ton U and reaching up to 65000 MW.day/ton U.

Four tests have been carried out with a 10 ms wide power pulse. The first 3 tests have been used as a reference to establish the French safety authority present position.

<table>
<thead>
<tr>
<th>DATE</th>
<th>TEST</th>
<th>BURN-UP (GWd/t)</th>
<th>OXIDE THICKNESS (μm)</th>
<th>ENTHALPY (cal/g)</th>
<th>RESULTS</th>
</tr>
</thead>
<tbody>
<tr>
<td>10.11.93</td>
<td>Na 1</td>
<td>62</td>
<td>80</td>
<td>116</td>
<td>Clad rupture and some fuel dispersion at 30 cal/g</td>
</tr>
<tr>
<td>10.06.94</td>
<td>Na 2</td>
<td>35</td>
<td>5</td>
<td>200</td>
<td>No failure</td>
</tr>
<tr>
<td>06.10.94</td>
<td>Na 3</td>
<td>52</td>
<td>50</td>
<td>117</td>
<td>No failure</td>
</tr>
</tbody>
</table>
The attached figure shows the experimental data points for the tests carried out at the end of 1994.

Determination of the threshold levels for rupture and energetic dispersion must be done with caution. Firstly, we are waiting for complementary experimental data in the areas of medium and high burn-up. Secondly, the dispersion of the mechanical characteristics within the irradiated clad material is a plausible cause for the observed phenomena and has not been taken into account yet.

Quantification of this effect will only be possible when a larger number of experimental data points for equivalent burn-up values are acquired.

Moreover, this figure does not allow us to distinguish possible influences on the defined threshold levels due to:

- The different clad materials for the fuel pins; the thresholds associated with the new generations of fuels (such as AFA 2G designed by Fragema) which are more resistant to corrosion in normal operation, could be superior to those of standard fuel.

- Within a fuel rod, the degree of corrosion will be different with the axial level.

Examining this figure, we can nevertheless estimate that, above a fuel burn-up of around 40,000 MW.day/ton U, the current safety criterion is probably no longer relevant.

New criteria should therefore be defined for high burn-up. In this respect, further improvements in our knowledge are required (new experimental data, understanding of the involved phenomena). An international level dialogue would be judicious.

III - SAFETY AUTHORITY POSITION

The position of the DSIN has been recently set out to EDF in a letter dated of the 15th of May 1995.

Before eventual new criteria are defined, the safety of operating plants must be grounded on the demonstration that a significant margin exists between the enthalpy likely to be reached in case of control rod ejection and the average fuel pellet enthalpy leading to dispersion.

In this respect, the elements provided by EDF to the French safety authority lead to believe that the safety of the 900 MWe PWR plants operated at the present time within the limit of the generic fuel assembly average burn up of 47000 MW.day/ton U, should not be affected.
Indeed, EDF calculations studies show that, on the basis of the most penalising fuel load pattern of the 900 Mwe French nuclear plants, the most irradiated rods would see their enthalpy raise up to a maximum of 60 cal/g in case of control rod ejection. The most irradiated rods are those for which the burn-up would reach, on average, 50000 MW.day/ton U with 54000 MW.day/ton U for the most irradiated pellets.

These values correspond with the authorised burn-up limit (47000 MW.day/ton U in assembly average).

Therefore an important margin exists between the enthalpy likely to be reached and the enthalpy leading to fuel dispersion (greater than 120 cal/g according to the results of the test REP Na3).

Further elements should also be taken into account: the small probability of the considered accident and the large uncertainties over the significance of its consequences.

All these elements are consistent with a safe operation of French PWR plants within the present generic authorisation limits. Nevertheless, the relevance of the generic authorisation must be confirmed. The experimental data base must be completed to verify that, for a local burn-up of around 54000 MW.day/ton U, the dispersion enthalpy is greater than the values for enthalpy deposit, calculated through a generic safety study not linked, as today, to a particular state of the nuclear sites.

EDF has requested an authorisation to overrun, during 1995, the burn-up of 47000 MW.day/ton U for several tens of assemblies distributed over 6 or 7 reactors (amongst the 13 listed in the same letter).

Similar arguments lead us to consider that the safety of the reactors concerned should not be significantly affected. Indeed, the maximum foreseen overrun leads to a burn-up per assembly of 48500 MW.day/ton U, which corresponds to a maximum local burn-up of 55000 MW.day/ton U.

In the event of a control rod ejection, the safety of these installations should not be compromised if we take into account the following considerations:

- the very small overrun of the authorised limit (47000 MW.day/ton U), thus, the margins which exist between the evaluations of the maximum energy reached in the fuel in the event of a control rod ejection and the current estimation of the energy values leading to the dispersion of the pellet remain important (greater than 60 cal/g),

- conservatisms in the hypothesis and the study methods (with the exception of the core configuration considered, which only covers the fuel load patterns used in the past) and in tests characteristics forming the data base, in particular the pulse width,
- at least, concerning the experimental fuel assembly for which the burn-up will exceed the authorised limit (47000 MW.day/ton) leading to a local burn up of 65000 MW.day/ton (for the CABRI sample), the safety remain ensured by positioning this fuel assembly in a central core position (without control rod), so that, in case of a control rod ejection, the linear power would be very low (less than 130 W/cm).

IV - FUTURE ACTIONS

The position of the French safety authority has not been questioned by the fourth CABRI test which was carried out after this position has been settled. This test has been performed on a sample made from the same rod as that used for the test REP Na1 (62000 MW.day/ton U) but taken from the lower portion of the rod and thus, having an initial oxide thickness of 20 μm. The test lead to a 110 cal/g energy deposition (with a 9 ms pulse) without any observed clad rupture.

The difference in the behaviour between the 2 samples could be linked to the presence of « hydrure » in the clad itself. This would be related to the behaviour of the oxide layer (presence of partial spalling).

Investigations into this subject are in progress with, in particular, a programme of analytical tests aimed at:

- better characterisation of the mechanical behaviour of highly irradiated clad material,
- more precise determination of clad/coolant exchange coefficients during the rapid insertion of reactivity,
- improvement of the understanding of the phenomena involved, particularly, within the RIM zone.

The CABRI programme continues according to the planning presented previously.
Regulatory Status on Transient Behavior of High Burnup Fuel and Related Research Activities in Japan

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Nuclear Safety Bureau  
Science and Technology Agency

Toshio FUJISHIRO  
Department of Reactor Safety Research  
Japan Atomic Energy Research Institute

1. Introduction

In Japan, safety regulations on nuclear facilities are performed through each step of site selection, design, construction and operation in accordance with the provisions of the Law for Regulations of Nuclear Source Material, Nuclear Fuel Material and Reactors (hereafter called LRNR) and the Electric Utility Industry Law. The direct safety regulations on the nuclear facilities are executed by the single competent authority such as the Ministry of International Trade and Industry and Science and Technology Agency according to the type of the facilities in various stages including permission of the installation, validation of the operation, inspection during operation and decommissioning of the facilities. The Nuclear Safety Commission (hereafter called NSC) provide advice and suggestions for inquiries relating to nuclear safety regulations as requested by safety authorities (Double Check).

At present, 49 commercial power reactors and 1 power reactor at the stage of development (prototype advanced thermal reactor) are in operation, and 3 commercial power reactors and 1 power reactor at the stage of development (prototype fast breeder reactor) are under construction. Today, total capacity of nuclear power generation in Japan reached approximately 40GWe.

The NSC established various guidelines to promote objectivity and rationality of the safety review processes (Double Check processes) relating to the installation permit of the nuclear facility. These guidelines will be improved properly based on the most recent scientific and technical knowledge and new ones will be followed as occasion demands.

2. The Safety Evaluation Guideline on the Reactivity-Initiated Events in Japan

The basic philosophy in the safety evaluation on the reactivity-initiated events for the light water power reactors in Japan was derived based on the various relevant knowledge such as the results from the reactor dynamics experiments, power excursion experiments and in-pile fuel destruction experiments and the experiences of RIAs such as that in SL-1.
The major purposes of the guideline consist of the followings;

(1) prevention of systematic fuel failure during an abnormal transient resulting from an uncontrolled reactivity insertion into the core represented by a continuous control rod withdrawal from the core at the critical state,

(2) prevention of mechanical energy generation to maintain coolable geometry in the core and to assure the integrity of reactor coolant pressure boundaries following fuel failure during a control rod ejection accident in a pressurized water reactor (PWR) or a control rod drop accident in a boiling water reactor (BWR), and

(3) protection of the pressure vessel and core internals against the mechanical influence exerted by the failure of waterlogged fuel rods.

The allowable fuel design limit for an abnormal transient during a postulated reactivity-initiated event is specified in terms of the maximum fuel enthalpies as a function of the fuel rod internal and external pressure difference as shown in Fig. 1. For an accident case of a reactivity-initiated event, the maximum fuel enthalpy is to be restricted below 230 cal/g. This safety guidelines was established in 1984 primarily based on the results from the NSRR experiments with fresh fuels as the test samples where the major fuel failure mechanism is melting of the materials such as Zircaloy and uranium dioxide at higher fuel enthalpy and embrittlement of the Zircaloy induced by the oxidation at higher temperature. The long-term burnup of a fuel rod in a power reactor may cause various physical phenomena in the rod such as embrittlement of the cladding material due to fast-neutron irradiation and oxidation at relatively low temperature. This might bring a different failure mechanism such as pellet-cladding mechanical interaction (PCMI) and a lower failure threshold. At that time when the guideline was established, technical knowledge was, however, not sufficient to determine a clear criterion to account for the effect of fuel burnup. Therefore, a provisional measure for the failure of burnup fuel (85 cal/g) was introduced based on the SPERT-CDC test results with the burnup fuel (approximately 32 MWd/kgU) to conservatively count the number of failed rods during an RIA and to evaluate the source term of radiation hazard. The NSRR experiments with burnup fuels as the test samples was also strongly recommended in the guideline.

3. Status of High Burnup Fuel Utilization in Japan

In Japan, extension of burnup in LWR fuels is one of the important R&D items to realize a long term and advanced utilization of LWRs. Current limitations in average discharge fuel assembly burnup are 50 MWd/kgU for BWR and 48 MWd/kgU for PWR. Japanese utilities are aiming at increasing discharge burnup up to 55 MWd/kgU in the near future. The first step of the program is to demonstrate the performances of improved fuels at high burnup through the irradiations in the real power reactors. To realize it with sufficient safety, the number of special fuel assemblies loaded in the core is limited to 8 at the maximum.
The first safety review report for this program was submitted by the MIT to the NSC on the 5th of January, 1995. It concerned the demonstration irradiation program by the Tokyo Electric Power Company at the units 1 and 2 (BWRs) of the Fukushima No.2 Nuclear Power Station. The NSC acknowledged this proposal on the 26th of June, 1995.

In this "Double Check" review by the NSC, the following understandings were essential to have the conclusion.

1. The increase of the fuel burnup in the assembly decreases power density in it.
2. The increase of the source term is still acceptable even when all of the special assemblies were assumed to be failed at an RIA.

The second safety review report concerned the program proposed by the Kansai Electric Power Company at the unit 4 (PWR) of the Ohi Nuclear Power Station was submitted by the MIT to the NSC and at present it is under review in the committee organized by the NSC.

4. Related Research Activities

The NSC is responsible for the promotion of nuclear safety research and settling a five-year program as well as the establishment of safety review guidelines. In the current program, research items related to the behavior of high burnup fuels during normal operation conditions and under accidental conditions have already been included. The NSRR experimental program with burnup fuels as test samples is a typical example.

As reported in this meeting, the NSRR experiments have been progressing and are giving us new knowledge on the transient behavior of burnup fuels under RIA conditions. One of the important findings from the NSRR experiments is that the PCMI could be one of the failure mechanisms in the burnup fuels as considered in the current safety guideline and it could reduce the failure threshold but the threshold is well above the provisional failure threshold of 85 cal/g when the fuel burnup is less than approximately 42 MWd/kgU.

Recently, technical information on the behavior of high burnup fuels (above 50 MWd/kgU) under RIA conditions became available by the progress of the NSRR experiments and the experiments performed in the CABRI reactor in France. It, however, seems to be not sufficient to derive any conclusion on the failure criterion for the high burnup case. The test fuels used in these experiments had a different design in cladding material from the rods in current usage and experienced prolonged special irradiations in the reactor environment. In this sense, these test rods could not be representative to identify the failure threshold although the test results gave us a good insight on the behavior of high burnup fuels under RIA conditions. For the realization of the burnup extension with sufficient safety, it is required to facilitate and extend the research in this
5. Summary

The burnup extension program in Japan is at the first stage where the small-scale demonstration irradiations of the improved assemblies are planned to show their performances under high burnup conditions. To realize burnup extension in the full core scale with sufficient safety, it is essential to understand the behavior of fuel rod with sufficiently high burnup under RIA conditions more exactly. For this purpose, this specialists meeting organized by OECD/NEA is quite important and give us an opportunity to have an insight for the direction of our future efforts.
Table 1 Status of Power Reactors in Japan

<table>
<thead>
<tr>
<th>Power Plants</th>
<th>Number of Facilities</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>Operating</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Commercial</td>
<td>49</td>
<td>GCR 1</td>
</tr>
<tr>
<td></td>
<td></td>
<td>PWR 22</td>
</tr>
<tr>
<td></td>
<td></td>
<td>BWR 26</td>
</tr>
<tr>
<td>Developmental stage</td>
<td>1</td>
<td>ATR 1</td>
</tr>
<tr>
<td>Under Construction</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Commercial</td>
<td>3</td>
<td>PWR 1</td>
</tr>
<tr>
<td></td>
<td></td>
<td>ABWR 2</td>
</tr>
<tr>
<td>Developmental stage</td>
<td>1</td>
<td>FBR 1</td>
</tr>
</tbody>
</table>

- Lowest prompt energy deposition that resulted in cladding failure.
- Highest prompt energy deposition that did not result in cladding failure.

![Graph showing Cladding Failure Limit as a Function of Fuel Rod Internal-external Pressure Difference](image)

Fig. 1 Cladding Failure Limit as a Function of Fuel Rod Internal-external Pressure Difference
Specialist Meeting on Transient Behaviour of High Burnup Fuel,
Cadarache, September 12 - 14, 1995

"German Licensing Approach and Consequences
for High Burnup Fuel"

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Abstract

This paper describes the main aspects of the German licensing practice and its consequences for fuel burnup increase. Planned and actually achieved fuel burnup values are given, and burnup dependent effects on fuel assembly and core design are discussed. The design bases are described for fast reactivity initiated accidents (RIA) in German PWRs and BWRs by control rod malfunctions and the safety requirements are specified. In view of the new experimental results of reduced fuel failure limits under RIA-condition, first results of investigations studying the burnup dependence of enthalpy rise are reviewed.

1 Introduction

There is a common world-wide trend in nuclear fuel utilization to increase fuel burnup. Increasing fuel enrichment and burnup in order to improve fuel cycle economy will influence safety related parameters. The objective of this paper is to describe the main aspects of the German licensing approach to guarantee nuclear safety.
2 Planned and actual fuel burnup values for German PWRs and BWRs

The German utilities have developed a long-term strategy to increase fuel burnup. The goal is to reach mean discharge burnup values of 50 MWd/kg until the middle of this decade and values of 50 - 55 MWd/kg for both LWR types at the begin of next century. Actually reviewed fuel designs for PWR are planned for a maximum mean fuel assembly burnup of 65 MWd/kg, which corresponds to an axially averaged fuel rod burnup of about 73 MWd/kg. For BWR fuel designs were licensed for a maximum mean fuel assembly burnup of 53 MWd/kg, which corresponds to an axially averaged fuel rod burnup of about 66 MWd/kg. The mean discharge burnup values achieved in operating plants are currently in the range of 35 - 42 MWd/kg.

3 Fuel licensing practice

Within the licensing procedures for nuclear fuel, two aspects /1, 2/ have to be considered:

1. Licensing of a new fuel assembly design, which significantly differs from fuel loaded in previous core loadings.

2. Approval of a specific core loading as basis for an operational permission.

Increasing fuel enrichment and burnup is an example of the first aspect. The fuel design will be licensed for use in operation if design reports comprising detailed descriptions and design evaluations, safety analyses including accident and transient analyses and test results have been reviewed, and if they confirm the fulfilment of safety requirements and the feasibility of safe operation of the reactor core and the plant using the new fuel design. This safety review will usually be performed and the license will be granted independently of the stringent time schedule of reloads.

Important for the licensing practice is the second aspect, namely that each specific core loading is approved before the operational permission for startup is granted. The safety review is based on design calculations determining safety-related parameters which depend on the core loading like power density distribution, shutdown margin,
and reactivity feedback conditions. The approved initial conditions for accident and transient analyses will be checked. In addition, the review will include results from inspections of the fuel assembly conditions, by visual inspections, measurement of leak-tightness or oxide layer thickness. Even particular safety concerns will be considered, like consequences of incidents reported from other plants that are relevant for safety of reactor operation.

4 Burnup dependence on fuel assembly and core design

Fuel burnup may indirectly affect the various areas of safety evaluation for fuel assembly design and core loading in different ways, e.g. by safety relevant parameters like cladding oxidation thickness, fission gas release or neutronic effects related to higher enrichment.

The mechanical design may be affected by

- the thermo-mechanical fuel behaviour at high burnup, e.g. rim-effect, fission gas release, failure models for LOCA analyses,
- the pci-behaviour of high-burnup rods,
- the clad corrosion of high-burnup rods.

An experimental data base is needed, as well as a validation of analytical models, for thermo-mechanical fuel behaviour and fuel assembly structural behaviour under operational and accidental conditions.

In addition to experimental programs in material test reactors, lead test assemblies may be inserted in operating plants to evaluate the behaviour of single fuel assemblies with high burnup rods before full discharge batches will reach the high burnup values.

The nuclear design may be affected by

- a lack of validation of calculational models for reactivity and power density distribution including isotopic inventories,
- a reduced shutdown reactivity margin due to reduced thermal absorber efficiency and stronger negative coolant temperature feedback,
- power density tilting in transition cycles,
- changes in the reactivity feedback.

These effects have to be considered in relation to following requirements: The calculational models have to be validated. The minimum shutdown reactivity margin in normal operation and in accident conditions must be guaranteed. The power density values are limited to avoid fuel failures during operation, and also to keep values within initial conditions for accident analyses. The inherent safety of core design must be assured, and the reactivity feedback coefficients should stay within ranges which were approved by accident analyses.

Various ways are possible to cope with these effects.

Measurements at the plant may be performed to determine reactivity characteristics at zero power or to determine power density distributions. Also specific examinations of lead test assemblies are possible to determine the isotopic inventory.

Adequate loading patterns can be chosen to increase shutdown reactivity margins and to reduce power density tilting. The efficiency of the shutdown system can be improved by increasing the boron concentration, e.g. in ECC-tanks, or by increasing the neutron absorbing B-10 content. The inherent safety conditions can be assured by limiting the boron concentration at BOC through extended use of burnable absorbers. Another way may be to review the assumptions of accident analyses, and, if necessary, adjustments of limitation and safety system setpoints are possible.

5 Accident analysis

Consequences of increased burnup of fuel assemblies on design basis accidents may be derived by influences on thermo-mechanical conditions or nuclear design parameters like reactivity feedback coefficients and shutdown reactivity margin. These aspects have been discussed above.
For reactivity initiated accidents due to control rod malfunctions the German rules and guidelines specify following general design requirements.

According to the RSK-Guidelines /3/, measures shall be taken to prevent an uncontrolled withdrawal of control assemblies. It shall be demonstrated that such an incident will nevertheless be controlled if it occurs.

To cope with the ejection of a control assembly, which may occur as a result of a guide tube or drive fracture, a second independent safeguard such as an intercepting device shall be provided apart from the safe design and thorough fabrication control of the guide tube unless the energy release resulting from the ejection of the control assembly with the greatest reactivity value will definitely not cause any damage to the reactor core and the reactor system. The fracture of a control assembly guide tube or a control assembly drive shall not cause any consequential damage to adjacent control assembly guide tubes or control assembly drives which would impair the functional reliability of other control assemblies. If such consequential damage cannot be precluded, it shall be demonstrated that even such an incident will not cause any damage to the reactor core.

In the Incident Guidelines /4/ the reactivity initiated accidents are classified as events without radiological impact due to adequate technical design provisions.

According to these requirements, it has been proven that fast reactivity initiated accidents caused by control rods will be limited by inherent nuclear feedback mechanisms and terminated by a scram activation before any fuel clad failure will occur in the fuel rod with highest power increase.

The following accident conditions are considered for PWR and BWR respectively: For PWR a complete rod ejection and the unintended control rod withdrawal at startup are analyzed for all operational conditions between hot zero power and nominal power. The worst conditions are given for hot zero power, but consequences for enthalpy rise are very limited because the maximum reactivity worth is low, about $2 \cdot 10^3$. In the calculations, fully 3D reactor core models have been applied. Additional investigations to determine the dependence on high burnup values will be performed.
For BWR a rod drop of 21 cm and the unintended control rod withdrawal at startup are analyzed. The rod drop is limited due to the particular design of the control rod drive mechanism, which limits the free dropping length to 21 cm, determined by the distances of notches in the guide tube.

The rod ejection is limited by an intercepting device to an even smaller distance. Therefore, the rod drop accident is the limiting accident condition for BWR. All cases should be analyzed for operational conditions between cold zero power and nominal power.

The assessment of accident consequences was based on the available experimental data from SPERT, PBF and NSRR test reactors. To exclude fuel cladding failures caused by reactivity initiated accidents a limit of peak fuel enthalpy of 170 cal/g, corresponding 711 kJ/kg, was applied. As mentioned, the German safety codes specify general requirements only, but this value was usually considered in technical specifications as limit to exclude fuel clad failures. In safety analyses a sufficient margin to this accepted limit of peak fuel enthalpy was confirmed. As the experimental results are based only on low and intermediate burnup values, the available margin was considered as a conservative approach related to burnup dependency.

Meanwhile, the new test results from CABRI and NSRR experiments /5/ confirmed the decrease of the failure threshold with increasing burnup. On the basis of the experimental results now available, the safety assessors request, that related to the burnup of the fuel rods the enthalpy rise in a RIA-event remains below the burnup dependent limit of fuel failures determined experimentally including the CABRI-Na-1 test result. The utilities presented calculations for the rod drop accident in BWRs, which show that the enthalpy rise in a fuel rod decreases significantly with increasing burnup of the affected fuel assemblies. Fuel burnup values have not yet reached values of 60 MWd/kg, but for a planned future equilibrium cycle with high burnup fuel, an adequate reduction of enthalpy rise below 15 cal/g for such high burnup rods has been calculated by the utilities.

The main criterion to assure that failure limits will not be exceeded seems to remain, as in the past, an adequate limitation of the maximum rod withdrawal reactivity rate. In addition to the usual electro-mechanical rod motion limitations, this has to be assured in every cycle by an appropriate core loading pattern. It is still in discussion and under
examination whether specific loading patterns may influence significantly the burnup related decrease in enthalpy rise in the representative loading patterns investigated up to now.

6 Conclusion

Because the increase of fuel burnup affects the fuel assembly and core design in many aspects, which have been discussed in this review.

Related to reactivity initiated accidents the design of German PWRs and BWRs includes specific countermeasures which limit consequences of such accident conditions.

The German licensing approach for reload assessment is based on safety analyses and reload specific calculations of safety relevant parameters, but also includes a detailed evaluation of operational experience and regular inspections of fuel assemblies. This approach contributes to assure safe operation of plants, when burnup will be increased step by step.

It is acknowledged that the actually performed experimental investigations determining the burnup dependent fuel failure limits for fast reactivity insertion contribute essential results to quantify the safety margins for such accidents.

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Session II: INTEGRAL TESTS AND ANALYSIS

Co-Chair: T. FUJISHIRO and M. HAESSLER

Summary of the Session............................................................... 55

« Behaviour of High Burnup PWR Fuel Under a Simulated RIA
Condition in the NSRR »
T. FUKETA, Y. MORI, H. SASAJIMA, K. ISHIJIMA, T. FUJISHIRO .............. 59

« Postulated Mechanisms on the Failure of 50 MWd/kgU PWR Fuel in the
NSRR Experiment and the Related Research Programs in JAERI »
K. ISHIJIMA, Y. MORI, T. FUKETA, H. SASAJIMA,............................... 87

« The Experimental Test Programme for the Study of High-Burnup PWR
Rods Under RIA Conditions in the CABRI Core »
M.C. ANSELMET-VITIELLO, F. ARREGHINI, M. HAESSLER ...................... 107

« Cladding and Fuel Modifications of a 60 GWJ/tM Irradiated Rod During
a Power Transient Performed in the CABRI Reactor »
P. MENUT, D. LESPIAUX, M. TROTABAS.......................................... 127

« The Behaviour of Irradiated Fuel Under RIA Transients :
Interpretation of the CABRI Experiments »
J. PAPIN, H. RIGAT, J.P. BRETON................................................ 137

« Development and Performance of a Research Programme for
the Analysis of High Burn-up Fuel Rods Behaviour under RIA Condition
in the IGR Pulse Reactor »
V. ASMOLOV, L. YEGOROVA..................................................... 155

« Primary Factors Causing the Failure of High-Burnup LWR Fuel Rods
During Simulated Reactivity Initiated Accidents »
R.K. McCARDELL, R.O. MEYER.................................................. 167

« Unexpected Transient on an Experimental Instrumented Fuel Rod in BR2 »
S. BODARD, B. COUPE, V. SOBOLEV, M. LIPPENS............................ 185

« STUDSVIK's Experience Related to LWR Fuel Behaviour at High Burnup »
H. MOGARD, M. GROUNES..................................................... 213
SUMMARY OF SESSION II: INTEGRAL TESTS AND ANALYSIS

Presentations

Nine (9) papers on integral tests and analysis including unexpected transient in an inpile experiment were presented.

Two (2) papers on NSRR test results and their interpretation were presented by JAERI. Results of pulse irradiation of 4 PWR rods irradiated to 50 GWD/MtU (HBO-1-4) and irradiated PWR type segment rods were introduced, and the failure mechanism was discussed.

Three (3) papers on the CABRI program, test results and analysis were presented by IPSN and CEA/DRN. Transient and PIE results of pulse irradiation of a 64 GWD/MtU PWR rod, failed at the fuel enthalpy of about 30 Cal/g (REP Na1) were introduced. Failure mechanism was discussed based on the interpretation of PIE results and code analysis of REP Na1 and also of other 4 tests followed (REP Na2-5).

Results and interpretation of high burnup Russian PWR rods irradiated at IGR pulse reactor were presented by NSI RRC-KI. Through the IGR tests, 13 WWER-1000 rods irradiated to 48 GWD/MtU, 10 rods with irradiated cladding and 20 fresh rods were irradiated in IGR by relatively slower pulse power than NSRR or CABRI tests. Failure mechanism and threshold were discussed.

Interpretation of major fuel failure mechanisms and the effects of cladding materials were introduced by INEL/USNRC, based on the interpretation of SPERT, PBF, NSRR, CABRI, IGR and other test results. Experience of fuel failure in an unexpected transient in experimental capsule of BR2 reactor, and that of ramping experiments for high burnup rods in Studsvik were introduced.

Main conclusions

Main conclusions obtained through the presentations and the discussion can be summarised as follows:

(1) Fuel Failure Mechanism

Common understanding is that the hydride-assisted PCMI failure mechanism is responsible for the failure of high burnup fuel rods under fast power transient of an RIA.

This type of failure has been observed for burnups higher than about 20 GWD/MtU in NSRR, CABRI, SPERT and PBF experiments.

The cracks in the cladding are initiated at the spots of local pre-existing hydriding, spot hydriding by spalling off of oxide layer and/or radially localised hydriding, at the surface during very early phase of the transient (few to some ten ms). The strong loading to the cladding may be enhanced by expansion
and liberation of fission gas at grain boundaries by quick temperature rise, in addition to thermal expansion of the pellets.

Different type of failure of high burnup fuel has been observed in IGR tests. Low level of oxidation (5–6μm) and absence of significant effects of cladding hydriding prevents PCMI failure. All the test rods failed by high temperature rupture of cladding, at the peak fuel enthalpy of 240 Cal/g for 48 GWd/t. However, direct comparison of the results is rather difficult due to large difference of power pulse width between IGR and other tests in NSRR, CABRI, SPERT and PBF.

Studies of the effects of power pulse width should be performed through experiments and analysis.

For much slower transient power rise of some ten seconds or larger, the failure mode will change to cladding oxidation or melting after DNB or by PCI failure, as were seen in BR-2 transient.

2) Effects of major parameters

• Fuel burnup

One of the dominant factors to control loading to the clad by fission gas expansion and by clad hydriding. The effects become more evident for over 40 GWd/MtU.

• Power Pulse Shape

There may exist the effects of pulse width to fuel failure mode and threshold. Strain rate in the pellet may become lower with wider pulse and milder grain boundary separation, resulting in milder loading to the cladding, for example.

Different understanding that the effect will be minor compared with clad hydriding is also presented.

• Cladding hydriding/oxidation

Local hydriding at cladding surface is thought to be important as the initiation sites of cracks. Spalling off of the oxidation layer at high burnup is one of the important causes of local heavy hydriding.

Embrittlement of the cladding with increase of average hydriding and high strain rate will also enhance the propagation of the cracks and may decrease the failure thresholds.

• Cooling conditions

For the failure mode of cladding rupture after DNB, high system pressure might have some influences due partly to suppressing DNB initiation and partly to decreasing pressure difference across the cladding. But no experimental evidence exists yet to quantify this effect.
(3) Failure thresholds

Fuel failure enthalpy decreases with burnup, especially above 40 GWd/MtU. The lowest value of failure enthalpy obtained in the integral tests are 85 Cal/g at 32GWd/MtU (SPERT), 60 Cal/g at 50 GWd/MtU (NSRR) and 30 Cal/g at 64GWd/MtU (CABRI).

Plots of fuel failure enthalpy vs. burnup are often used to indicate the effects of burnup. But one should be careful when applying in a quantitative manner without consideration of the experimental conditions.

These values are dependent on the experimental conditions i.e. fuel design, irradiation conditions, system pressure and temperature, pulse width, etc.

(4) Application to reactor conditions

Integral tests are not fully representative of the actual reactor conditions. Ample consideration should be taken when applying the test results to actual reactor conditions. Major conditions to be considered are:

- System conditions (coolant pressure, temperature and flow)
- Transient power history (power pulse width)
- Fuel conditions (fuel design, irradiation conditions).

Since the crack initiation in the cladding is strongly influenced by local hydriding, it is expected that modification of clad material against oxidation (low tin, Nb addition, etc.) may largely improve the strength against PCMI failure, but no firm experimental evidence yet exists.

(5) Fission product release

FP release from the failed rod to the coolant is important for the estimation of environmental impact. NSRR and CABRI test results indicated that FP release is a strong function of fuel burnup. The correlation is to be further developed for higher burnup region for better estimation.

(6) Fuel fragmentation and consequences

Release of finely fragmented fuel particles at fuel failure has been observed in CABRI and NSRR tests. As a consequence of the fuel dispersal, partial blockage of the flow channel occurred in the CABRI test, but no significant mechanical energy generation has been detected at the energy deposition range reported. It is suggested, however, that vigorous steam generation resulting in significant mechanical energy may occur when fine fuel fragments are released into the coolant especially at high temperature at higher energy deposition.
Remaining issues

From the safety point of view, the two more important aspects of fuel behaviour are fuel cladding failure and the resulting fission product release, and fuel fragmentation which may threaten core coolability and the integrity of the reactor primary system.

(1) Fuel failure

- Quantify uncertainties
  - Experiments: measurements
  - Analysis: code validation, input qualification

- Application to reactor conditions
  - Coolant conditions
  - Fuel design
  - Burnup range
  - Power shape, etc.

(2) Fuel fragmentation and consequences at high energy depositions

- Experimental data from integral tests and interpretation.
Results obtained in the NSRR power burst experiments with irradiated PWR fuel rods with fuel burnup up to 50 MWd/kgU are described and discussed in this paper. Data concerning test method, test fuel rod, pulse irradiation, transient records during the pulse and post irradiation examination are described, and interpretations and discussions on fission gas release and fuel pellet fragmentation are presented. During the pulse-irradiation experiment with 50 MWd/kgU PWR fuel rod, the fuel rod failed at considerably low energy deposition level, and large amount of fission gas release and fragmentation of fuel pellets were observed.

INTRODUCTION

To provide a data base for the regulatory guide of light water reactors, behavior of reactor fuels during off-normal and postulated accident conditions such as reactivity-initiated accident (RIA) is being studied in the Nuclear Safety Research Reactor (NSRR) program of the Japan Atomic Energy Research Institute (JAERI). Numerous experiments using pulse irradiation capability of the NSRR have been performed to evaluate the thresholds, modes, and consequences of fuel rod failure in terms of the fuel enthalpy, the coolant conditions, and the fuel design. The current safety evaluation guideline for the reactivity-initiated events in light water reactors (LWRs) was established by the Nuclear Safety Commission of Japan in 1984 based mainly on the results of the NSRR experiments. In the guideline, an absolute limit of fuel enthalpy during an RIA is defined as 963 J/g fuel (230 cal/g fuel) to avoid mechanical forces generation. The guideline also defines an allowable limit of fuel enthalpy for fuel design as a function of difference between rod internal pressure and system pressure. When fuel rod internal pressure is lower than external pressure, the limit is 712 J/g fuel (170 cal/g fuel). All of the NSRR data used for the guideline were limited to those derived from the experiments with fresh, i.e. un-irradiated fuel rods. For this reason, the current Japanese guideline adopted a peak fuel enthalpy of 356 J/g fuel (85 cal/g fuel) as a provisional failure threshold of pre-irradiated fuel rod during an RIA; and this failure threshold is used to evaluate number of failed pre-irradiated fuel rods, and to assess source term regarding fission gas release in a postulated RIA. This failure threshold enthalpy of 356 J/g fuel was derived from only one experiment, i.e. the test 859 performed in the Special Power Excursion Reactor Test program in the Capsule Driver Core facility (SPERT/CDC). Hence, the current guideline noted that the failure threshold should be revised by the NSRR experiments with pre-irradiated fuel rods.

Because only limited number of pre-irradiated fuel rods were tested in the SPERT/CDC and the Power Burst Facility program (PBF) both in the Idaho National Engineering Laboratory in the United States, the failure threshold of pre-irradiated fuel

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**Behavior of High Burnup PWR Fuel**

Under a Simulated RIA Condition in the NSRR

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59
rods was not statistically established, and failure mechanism of a pre-irradiated fuel rod during an RIA remains undetermined. In addition to the requirements for the regulation, the burnup effect becomes one of a primary concern in the field of fuel behavior study since economics and prudent utilization of natural resources have provided strong incentives for extending the burnup levels of fuel operating in commercial power-producing LWRs. The burnup limits in Japan have been increased from 39 MWd/kgU to 48 MWd/kgU for PWRs and 50 MWd/kgU for BWRs, and further increase of the limits to 55 MWd/kgU is in consideration.

In these conditions, a series of experiments with pre-irradiated fuel rods were newly initiated in July 1989 as a part of the NSRR program after the completion of necessary modifications of the experimental facilities. The objectives of this program are to investigate fuel behavior of pre-irradiated fuel rod under the simulated RIA conditions; to determine the fuel rod failure threshold of pre-irradiated fuel rod, to clarify the influences of the fuel burnup, and to clarify the modes, mechanisms and consequences of failure of pre-irradiated fuel rod. The test fuel rods to be subjected to the pulse irradiations in the NSRR include segmented fuel rods refabricated from full size fuel rods of commercial power reactors (PWRs and BWRs), and short fuel rods preirradiated in the Japan Materials Testing Reactor (JMTR) of JAERI. This paper describes the results obtained from the NSRR experiments with irradiated PWR fuels, including 50 MWd/kgU PWR fuels (the HBO fuels), and focuses on fission gas release and fragmentation of fuel pellets. The occurrence of cladding failure and its mechanism are presented in separate paper. However, the data appeared in these papers have relatively large uncertainty in terms of energy deposition, fuel enthalpy, fission gas release etc., since these documents have mainly focused on presenting status of the NSRR program as interim reports. Re-evaluated and accurate data will be presented here in this paper.

EXPERIMENTAL METHOD AND TEST CONDITION

Nuclear Safety Research Reactor (NSRR)

The NSRR is a modified TRIGA-ACPR (Annular Core Pulse Reactor) of which salient features are the large pulsing power capability which allows the moderately enriched fuel to be heated by nuclear fission to temperature above the melting point of UO. and large (22 cm in diameter) dry irradiation space located in the center of the reactor core which can accommodate a sizable experiment. The general arrangement of the NSRR is shown in Fig. 1. The core structure is mounted at the bottom of a 9 m deep open-top water pool, and cooled by natural circulation of the pool water. Access to the experimental cavity is provided by vertical and an offset loading tubes which are jointed together in a "Y" fitting above the core as shown in the figure.

The NSRR core consists of 149 driver uranium-zirconium hydride (U-ZrH) fuel/moderator elements, 6 fuel follower regulating rods and 2 fuel follower safety rods. The pulsing operation is made by quick withdrawal of enriched boron carbide transient rods by pressurized air. The pulsing power escalation is controlled by spectrum hardening caused by the moderator temperature increase, and the Doppler effect in the NSRR. During the maximum reactivity insertion of 3.4% Δk/k ($4.67), the pulse power reaches 21.1 GW with a corresponding core energy release (integrated reactor power) of 117 MJ with a minimum reactor period of 1.17 ms. The energy deposition in a test fuel rod is controlled by the amount of reactor power and by the enrichment of the fuel. With the maximum pulse, approximately 2100 J/g fuel (500 cal/g fuel) can be deposited in a 10% enriched fresh fuel rod contained in a single-wall test capsule. The detailed description of the NSRR was provided by Saito et al.
Pulse Irradiation

Figure 2 shows NSRR power histories of $4.6, $3.6$ and $3.0$ reactivity insertion, which are recorded during tests HBO-3, -4 and -2, respectively. Shape of reactor power history depends on the inserted reactivity, and the smaller pulse becomes broader. While the full width at half maximum (FWHM) in $4.6$ pulse is $4.4$ ms, that in $3.0$ pulse is $6.9$ ms. Figure 3 shows the FWHM and integrated reactor power as a function of inserted reactivity. Since the integration circuit for reactor power is cut off $1$ s after the initiation of reactor operation in the NSRR data acquisition system, only the integrated reactor power during initial $1$ s of reactor operation (NVT$_1$) can be known experimentally. The integrated reactor power including power during runout phase (NVT$_\infty$) is hence obtained from an analysis with EUREKA code.

Energy Deposition and Peak Fuel Enthalpy

The energy deposited to a test fuel during pulse irradiation, is a key attribute among test conditions, which represents magnitude of power burst. To evaluate the energy deposition, number of fissions generated during the pulse irradiation is obtained from gamma–ray measurement of sample solution from post–pulse fuel pellet. Because additional burnup during the pulse irradiation is much smaller than that accumulated during base irradiation in a commercial reactor or the JMTR, only short life fission products are used for evaluating the number of fissions during the pulse irradiation. Fission product Ba–140, with a half life of $12.75$ days, is selected for the evaluation. In order to reduce high gamma ray background from Cs–137 and other fission products, chemical separation scheme is applied to the sample solution. A ratio between number of fissions in unit mass of a test fuel and NVT$_\infty$ is constant in different experiments as far as the test fuel specifications, e.g. fuel burnup, initial enrichment, dimension, etc., and test setup, e.g. irradiation capsule, coolant subcooling, etc., are identical. Unless otherwise noted, the "energy deposition", Q$_t$, denotes the radial average total energy deposition per unit mass of fuel (J/g fuel or cal/g fuel) in this paper. Since the energy deposition includes an energy released during runout phase, one should know an amount of energy promptly generated at pulse for assessment of fuel behavior. The prompt energy deposition, Q$_p$, can be calculated by using a ratio Q$_p$/Q$_t$, which is provided from the EUREKA analysis as a function of an inserted reactivity. Since the NSRR transient is extremely fast, the prompt energy deposition becomes identical to peak fuel enthalpy under adiabatic assumption.

Test Capsule and Instrumentation

The experimental capsule used in the pulse irradiation is a newly developed double−container system for the irradiated fuel rod test in the NSRR. Figure 4 shows a schematic diagram of the capsule. The outer capsule is a sealed container of $130$ mm in inner diameter and $1,250$ mm in height, and the inner capsule is a sealed pressure vessel of $72$ mm in inner diameter and $680$ mm in height. In terms of the design of the capsule, the easiness of assembling and disassembling works by the remote handling system is one of primary concern as well as the structural strength. The capsule contains an instrumented test fuel rod with stagnant water at atmospheric pressure and ambient temperature.

The instrumentation used in the pulse irradiation is illustrated in Fig. 5. Cladding surface temperatures are measured by $0.2$ mm bare–wire R type (Pt/Pt–13%Rh) thermocouples (T/Cs) spot–welded to the cladding at three elevations. Coolant water temperature is measured by sheathed K type (CA) thermocouples ($1$ mm in diameter) near the cladding surface at top of the test fuel rod and/or center of the fuel stack. A strain gauge type pressure sensor is installed at the bottom of the inner capsule to measure the increase of capsule internal pressure. A foil type strain gauge was attached at axial center on the outer surface of the inner capsule wall to measure the deformation of capsule.
some experiments, sensors for axial elongations of pellet stack and cladding tube are
instrumented.

Test Fuel Rod

In a series of the irradiated PWR fuel experiments, four different test fuels have been
refabricated from full-size commercial reactor fuels, and subjected to the pulse irradiation
in the NSRR. The test fuels consist of the MH, GK, OI and HBO test fuels (acronyms
for the fuels irradiated in the Mihama, Genkai, Ohi reactors and High Burnup fuels
irradiated in the Ohi reactor, respectively). Irradiation history in the commercial reactor
is a key attribute in this program. Fuel burnup and linear heat generation rate (LHGR)
during the base-irradiation (the irradiation in each commercial reactor or the JMTR) are
listed in Table 1. Preceding to the extension of PWR fuel burnup limit from 39
MWd/kgU to 48 MWd/kgU, the demonstration program of high burnup fuel had been
performed in the Ohi unit #1 reactor. The HBO test fuel had been irradiated in this
program, and the fuel burnup reached 50.4 MWd/kgU.

As-fabricated, initial fuel specifications are listed in Table 2. The MH and GK fuels
are 14×14 PWR type, and have the same dimensional configurations. While the OI
and HBO fuels are 17×17 type, they have different cladding thickness and pellet outer
diameter since fuel manufacturer of each fuel is different. It should be noted that the
HBO fuel was not newly designed and manufactured for the high burnup application.

Dimensional data of the short-sized test fuel rods are listed in Table 3. The radial
distance between cladding inner surface and fuel pellet (P/C gap) listed in the table is
obtained from metallography for arbitrary horizontal cross-section (round slice). As it can
be seen in this table, the P/C gap of the HBO test fuels is smaller than those of the other
test fuels, since creep down of the cladding exceeded and the linear heat generation rate
in the last irradiation cycle is lower in the high burnup HBO test fuel. As an example,
the HBO test fuel rod is illustrated in Fig. 6.

The information regarding the test fuel preirradiated in the JMTR is also listed in
Tables 1, 2 and 3. Because of the limitation of the NSRR pulsing capability and the low
residual fissile in the irradiated commercial reactor fuel, the maximum fuel enthalpy in
the experiments with the irradiated commercial reactor fuels is restricted to 460 J/g fuel
(110 cal/g fuel) or lower. On the other hand, the fuel rods subjected to the pre-irradiation
in the JMTR, JM test fuel, contain the fuel initially enriched to 10% or 20%. This
relatively high initial enrichment of the JM test fuel realizes the higher fuel enthalpy
during the pulse irradiation in the NSRR. The P/C gap of the JM test fuel keeps almost
same value through the pre-irradiation, since the JM test fuel is irradiated in the capsule
containing helium gas at atmospheric pressure.

Pre-test condition of the HBO test fuel

Figure 7 shows a burnup distribution calculated for the HBO fuel by using
RODBURN code.(21) The RODBURN code is a combination of the burnup analysis code
ORIGEN(22) and the resonance integral code RABBLE(23), and provides radially-localized
burnup distribution in fuels. The local burnup predicted for the HBO fuel reaches 82
MWd/kgU or higher in the outer 60 μm region where local burnup is enhanced by
plutonium production and fissioning. The structural change can be visually observed in
this outer 60 μm region during pre-pulse fuel examinations performed on specimens from
the HBO fuel. Figure 8 shows fuel pellet periphery of the HBO fuel horizontal cross-
section. The outer 60 μm region is characterized by loss of optically definable grain
structure and high concentration of small porosity. This region is termed the "rim" region.
The concentration of small porosity, less than 4 μm² in area, is increased in fuel
periphery, as shown in Fig. 9.

The state of the cladding of the HBO fuel is described in the separate paper(15) in
combination with possible decrease of the integrity.
Pulse-irradiation Condition

The pulse-irradiation conditions including the energy deposition and peak fuel enthalpy are listed in Table 4. The Tests HBO-1, HBO-3 and OI-2 were performed with the maximum pulse of the NSRR. Because of high burnup and small number of residual fissile in the HBO fuel, peak fuel enthalpy was restricted to 310 J/g fuel (74 cal/g fuel). The Test KF-1 (referred as GK-3 in some documents) is excluded from the table, since the experiment is atypical. The fuel rod used in the Test KF-1 was exposed to excessive load follow operations with several hundred cycles in the JMTR.

Based on the local burnup information from the RODBURN code analysis, a preliminary calculation regarding radial power distribution at pulse is being made with TWOTRAN code. Figure 10 gives a radial power profile of the HBO test fuel during pulse-irradiations. Considerably high peaking is predicted, and the radial peaking factor reaches about 2.7 at fuel periphery. The corresponding local peak fuel enthalpy in the Test HBO-1 is about 820 J/g fuel (200 cal/g fuel), and fuel pellet surface temperature is about 2600 K (2330 deg C) according to the MATPRO data. The fuel center-line temperature remains about 1000 K (730 deg C).

RESULTS AND DISCUSSION

Appearance of Post-test Fuel Rod

The first test in a series of experiments with the HBO fuels, the Test HBO-1, resulted in fuel failure, while the other irradiated PWR experiments, including the Tests HBO-2, -3 and -4, remained no failure. Figure 11 shows post-test appearance of the HBO-1 test fuel. Axial cracking of the cladding in entire region corresponding to the fuel stack occurred. The fractures are similar to those occurred by hydride-assisted PCMI in the SPERT 859 experiment. All of fuel pellets did not stay inside the rod, and were found in the capsule water as fragmented debris. Since the collected fuel pellets are finely fragmented, it can be thought that the fuel pellets are expelled from the fractured opening during the pulse. However, it can be also expected that the pellets dropped from the horizontal break after the pulse. The break can be seen at the bottom end of fuel active region.

Transient Record

Figure 12 illustrates the transient records of the reactor power, cladding surface temperature, fuel rod internal pressure and capsule internal pressure during the pulse-irradiation of the Test HBO-1 with the peak fuel enthalpy of 305 J/g fuel (73 cal/g fuel). Thermo-couple failure and spikes in capsule and fuel rod internal pressure histories observed simultaneously. This indicates an occurrence of cladding failure. The energy deposition at failure is approximately 250 J/g fuel (60 cal/g fuel). The early failure when the cladding surface temperature remains about 50 deg C indicates cladding cracking caused by pellet cladding mechanical interaction (PCMI). Although all of the fuel pellets was expelled or dropped from the rod and was recovered as finely fragmented debris, the transient records did not show pressure generation indicating an occurrence of molten fuel-coolant interaction. Because of the fuel failure in the Test HBO-1, pellet stack and cladding elongations were not successfully measured. The transient records of the elongations in the Test HBO-2 with the peak fuel enthalpy of 157 J/g fuel (37 cal/g fuel), as shown in Fig. 13, indicate rigid adhesion between the fuel pellets and the cladding, resulting in considerable PCMI.

Fuel Deformation

Residual hoop strain was obtained from dimensional measurements on the post-test fuel rods. The residual hoop strain is shown in Fig. 14 as a function of the peak fuel enthalpy. The strain becomes larger in the increased peak fuel enthalpy. In relatively low peak fuel enthalpy range, 250 J/g fuel (60 cal/g fuel) or lower, the Tests HBO resulted in...
in larger strain than those in the Tests MH, as it was expected from the thinner pre-pulse P/C gap in the HBO test fuel. On the other hand, the strain of the Test HBO-3 with the peak fuel enthalpy of 310 J/g fuel (74 cal/g fuel) was almost same with that in the Test MH-3 with the peak fuel enthalpy of 280 J/g fuel (67 cal/g fuel). It should be noted that the Tests GK-1 and OI-2 resulted in no failure although the strains in these experiments exceeded 2% and 4%, respectively.

Fission Gas Release

After the pulse irradiation, rod-average fission gas release was destructively measured for the test rod by rod puncture and gas analysis. The fission gas release during the pulse-irradiation is shown in Fig. 15 as a function of the peak fuel enthalpy. Fission gas release from the HBO fuel during base-irradiation was 0.49%. On the other hand, significant fission gas release occurred in the pulse-irradiation of the Tests HBO. Fission gas release is 17.7% even in the Test HBO-2 with the peak fuel enthalpy of 157 J/g fuel (37 cal/g fuel), and reaches 22.7% in the Test HBO-3. It should be noted that the fission gas release in the Test HBO-2 is higher than the release in the Test GK-1 with the peak fuel enthalpy of 389 J/g fuel (93 cal/g fuel). Figure 16 shows the fission gas release as a function of the fuel burnup. Higher fuel burnup correlates with the higher fission gas release. Figure 17 shows the fission gas release as a function of the fuel pellet volumetric swelling obtained from dimensional data of the post-test rods by assuming the pre-pulse P/C gap listed in Table 3. The significant fission gas release in the Tests HBO occurred with relatively small fuel swelling.

Fuel Pellet Structural Change

Metallographical examinations on the post-test HBO fuel rods are being extensively performed. Since the fuel pellets of the Test HBO-1 were finely fragmented, horizontal round slice and vertical division could not be sampled from the test fuel. As for the Tests HBO-2, -3 and -4, round slices and vertical divisions were subjected to the examinations. Figure 18 shows horizontal cross-section of the Test HBO-3 fuel. Number of radial and circumferential cracks can be seen in the fuel pellet periphery. The radial crackings in the peripheral region were observed also in other irradiated fuel experiments, i.e. MH, GK OI and JM test series. On the other hand, the post-test HBO fuel is characterized by the circumferential crackings. The vertical cross-section of the Test HBO-3 fuel is shown in Fig. 19. Number of cracks can be seen in the vicinity of the dish. The crackings seem stream-lines between source and sink in both ends of the dish, or magnetic-lines between positive and negative poles. A part of fuel pellet was collapsed during the pulse-irradiation. X-ray photograph showing the post-test HBO-3 fuel rod indicated that the collapse of fuel pellets in both ends of the fuel stack.

Fuel Pellet Fragmentation

The fuel pellets were found as finely fragmented particles after the Test HBO-1. A particle size distribution of the Test HBO-1 fuel is given in Table 5. The fragmented fuel debris were sieved to obtain the particle size distribution. Since the fuel was highly radioactive, the variation of mesh size was restricted to only two, and the mesh openings for the sieves were 500 μm and 50 μm. The result shows an occurrence of intensive fragmentation. About 90% of recovered particles are smaller than 500 μm, and a half or more is smaller than 50 μm. Regarding destructive forces generation, fragmented particle size distribution has been examined also in NSRR high energy deposition experiments with fresh, un-irradiated fuels. In the fresh fuel experiment with an energy deposition of 1600 J/g fuel (380 cal/g fuel) or higher, partly molten fuel ejected from the rod and fragmented fuel particles were recovered. As for fresh fuel rods, more than a half of debris are larger than 100 μm even in the experiment with an energy deposition of 2100 J/g fuel (500 cal/g fuel). The fuel recovered in the Test HBO-1 became the
finest particles in the NSRR program. Optical and scanning electron microscopy (SEM) and electron probe microanalysis (EPMA) are in planning stage for relatively large fragmented fuel particle.

The radially averaged peak fuel enthalpy in the Test HBO-1 is only 306 J/g fuel (73 cal/g fuel), and the predicted fuel temperature is about 2600 K (2330 deg C) at maximum, which is well below the melting point. It is naturally accepted that the fuel pellets of the Test HBO-1 have not melted during the experiment. During the PIE process, once-molten, spherical particle was not observed. Hence, one can hardly expect an occurrence of molten fuel–coolant interaction, or steam explosion. However, it seems premature to deny the possibility of mechanical forces generation caused by vigorous boiling. Since surface area of the finely fragmented fuel particles is considerably large, prompt contact of the particles with coolant water may generate mechanical force, especially under stagnant and high subcooling coolant and atmospheric pressure conditions of the NSRR experiment.

Fragmentation of irradiated fuels was observed also in recent JM-14 and JMH-3 experiments with the energy depositions of 840 J/g fuel (200 cal/g fuel) or higher. Axial cracking and fuel fragmentation similar to the Test HBO-1 occurred in the both tests. Particle size distributions in the tests will be examined.

Grain Boundary Separation

Grain boundary separation was observed initially in the Test JM-4\textsuperscript{14} with fuel burnup of 21.2 MWd/kgU and peak fuel enthalpy of 743 J/g fuel (177 cal/g fuel), and in subsequent JM-5 experiment with fuel burnup of 25.7 MWd/kgU and peak fuel enthalpy of 697 J/g fuel (167 cal/g fuel). In these experiments, secondary electron images of post-pulse fuel pellets show the occurrence of significant grain boundary separation in extensive area. The grain boundary separation could contribute to the significant swelling of the fuel pellets and fission gas release in these experiments. The results suggest that the whole amount of fission gas accumulated in the grain boundary may be released during the pulse irradiations. The significantly large fission gas release, the fuel fragmentation producing extremely fine particles and pre-existing small porosity in fuel pellets of the Test HBO-1 also indicate the grain boundary separation occurred almost instantaneously. Figure 20 illustrates the postulated scheme. Fission gas pores are accumulated along grain boundaries during the base–irradiation. Rapid expansion of fission gas accumulated in the small pores may cause weakening of the boundaries and subsequent grain boundary separation, and then results in fission gas release and fuel fragmentation.

Fuel Failure

In the NSRR experiments performed so far, fuel rod failures have occurred in seven experiments, i.e. the Tests HBO-1, JM-4, JM-5, JM-12, JM-14, JMH-3 and JMN-1. Figure 21 summarizes fuel burnup of subjected test fuels and peak fuel enthalpy during transients in RIA experiments.\textsuperscript{10,28} Fuel integrity have been demonstrated at the peak fuel enthalpy below 450 J/g fuel (108 cal/g fuel) for the fuel burnup of 42 MWd/kgU or lower. The data in the figure, however, suggests decreased failure threshold in high burnup region in terms of peak fuel enthalpy. Fission gas accumulation and its rapid expansion may contribute to the significant fuel swelling which has been observed in the NSRR experiments. The swelling has a potential to cause PCMI, and possibly in combination with decreased cladding integrity, to generate the cladding failure. In a series of experiments with the JM test fuels, pre-existing hydride blister in the cladding played important roles in the failure of the rods. In particular, axial crackings over fuel active region were observed recent Tests JM-14 and JMH-3.

SUMMARY AND CONCLUSIONS

The Test HBO-1 with a 50 MWd/kgU PWR fuel resulted in fuel failure at the energy
deposition of approximately 250 J/g fuel (60 cal/g fuel). The results suggest possible reduction of failure threshold for high burnup fuels, and indicate that PCMI with swelling of the fuel pellets leads to the failure. Rapid thermal expansion of accumulated fission gas can intensify the swelling and fission gas release, and subsequent fuel fragmentation to extremely small particles.

The 50 MWd/kgU fuel rods in three experiments following to the Test HBO-1 survived through the transients with peak fuel enthalpy ranged from 157 to 310 J/g fuel (37 to 74 cal/g fuel). However, significant fission gas release up to 22.7% occurred.

Further investigations on fuel failure mechanisms through in-pile integrated experiments, out-of-pile separate effect tests and phenomenological modeling could contribute to "accident-conscious" fuel design to avoid fuel failure and excessive fission gas release.

Acknowledgements

The HBO test series have been performed as a collaboration program between JAERI and Mitsubishi Heavy Industries, LTD. by using fuel rods transferred from Kansai Electric Power Company. The authors would like to acknowledge and express their appreciation for the time and effort devoted by numerous engineers and technicians in Reactivity Accident Research Laboratory, NSRR Operation Division, Department of Hot Laboratories, Department of JMTR Project and Analytical Chemistry Laboratory, JAERI. They also acknowledge the support and help of individuals and other organizations too numerous to cite, whose contribution were critical to the success of the program.

REFERENCES


Table 1 Base-irradiation conditions

<table>
<thead>
<tr>
<th>Test Fuel ID</th>
<th>Reactor</th>
<th>Initial Enrichment (%)</th>
<th>Irradiation cycle</th>
<th>Start of irradiation</th>
<th>End of irradiation</th>
<th>Fuel burnup (MWd/kgU)</th>
<th>LHGR (kW/m)</th>
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<td>HBO</td>
<td>Ohi unit #1</td>
<td>3.2</td>
<td>4</td>
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<td>OI</td>
<td>Ohi unit #2</td>
<td>3.2</td>
<td>2</td>
<td>Dec., 1985</td>
<td>Aug., 1988</td>
<td>39.2</td>
<td>20.7</td>
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<td>MH</td>
<td>Mihama unit #2</td>
<td>2.6</td>
<td>4</td>
<td>June, 1978</td>
<td>Aug., 1983</td>
<td>38.9</td>
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<td>GK</td>
<td>Genkai unit #1</td>
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<td>3</td>
<td>Feb., 1975</td>
<td>Feb., 1979</td>
<td>42.1</td>
<td>20.1</td>
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<td>JM</td>
<td>JMTR</td>
<td>10 or 20</td>
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<td>12 to 40</td>
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Table 2 As-fabricated fuel rod specifications

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<tr>
<th>Fuel ID</th>
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<th>Cladding</th>
<th>Fuel pellet</th>
<th>Radial P/C gap</th>
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<td>I.D.</td>
<td>Thickness</td>
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<td>JM</td>
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(unit: mm)

Table 3 Test fuel rod specifications

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<th>Test Fuel ID</th>
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<td>135</td>
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<tr>
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Table 4 Pulse-irradiation condition

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<th>Fuel Burnup (MWd/kgU)</th>
<th>Date of Pulse</th>
<th>Inserted Reactivity ($)</th>
<th>Energy Deposition (J/g fuel) (cal/g fuel)</th>
<th>Peak Fuel Enthalpy (J/g fuel) (cal/g fuel)</th>
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<td>Mar. 25, 1994</td>
<td>3.0</td>
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Table 4 Particle size of recovered fragmented fuel in the Test HBO-1

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<th>% in initial mass</th>
<th>% in total collected</th>
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<tr>
<td>d ≥ 500 μm</td>
<td>4.78</td>
<td>6.5</td>
<td>9.7</td>
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<tr>
<td>500 μm ≥ d ≥ 50 μm</td>
<td>17.81</td>
<td>24.3</td>
<td>36.0</td>
</tr>
<tr>
<td>50 μm ≥ d</td>
<td>26.84</td>
<td>36.7</td>
<td>54.3</td>
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<tr>
<td>Total collected</td>
<td>49.43</td>
<td>67.5</td>
<td>100</td>
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<tr>
<td>Un-collected fuel pellets</td>
<td>23.78</td>
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<tr>
<td>Initial mass</td>
<td>73.21</td>
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<td>100</td>
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Fig. 1 Schematic diagram of the NSRR vertical cross-section
Fig. 2  Power histories during pulse-irradiations for the Test HBO–2, –3 and –4

Fig. 3  Integrated reactor power (core energy release) and the full width at half maximum (FWHM) as a function of inserted reactivity
Fig. 4  Schematics of experimental capsule for pulse-irradiation

Fig. 5  Instrumentations for pulse-irradiation experiment
Fig. 6  Schematics of the HBO test fuel (segmented 50 MWd/kgU PWR fuel)
Fig. 7 Radial burnup distribution of the HBO fuel
Fig. 8 HBO fuel horizontal cross-section (before pulse, as-etched)
Fig. 9  Number density of porosity in the HBO fuel (before pulse)

Fig. 10  Radial power profile of the HBO test fuel at pulse
Fig. 11 Post-test appearance of the HBO–1 test fuel
Fig. 12 Transient records of reactor power, cladding surface temperature, fuel rod internal pressure and capsule internal pressure during the pulse-irradiation of the Test HBO-1
Fig. 13  Transient records of elongations and cladding surface temperature in the Test HBO–2

Fig. 14  Residual hoop stain as a function of peak fuel enthalpy
Fig. 15  Fission gas release during the pulse-irradiation as a function of the peak fuel enthalpy

Fig. 16  Fission gas release as a function of fuel burnup
Fig. 17 Fuel pellet volumetric swelling vs. fission gas release
Fig. 18 Metallography showing horizontal cross-section of the post-test HBO-3 fuel
Fig. 19  Metallography showing vertical cross-section of the post-test HBO-3 fuel
As-fabricated Fuel Pellet

After Base-irradiation
Re-crystallization and Increase of intergranular bubbles due to extended burnup

Pulse Irradiation
Rapid expansion of gas in intergranular bubbles and acceleration of fuel swelling

Grain Boundary Separation and Fission Gas Release

Fig. 20 Postulated grain boundary separation resulting in pellet swelling, fission gas release and fuel fragmentation
Fig. 21  Fuel burnup and peak enthalpy in irradiation fuel experiments
1. Introduction

Since 1993, in-pile experiments with high burnup PWR fuel (50 MWd/kgU) under simulated reactivity-initiated accident (RIA) conditions (HBO test series) were initiated in the Nuclear Safety Research Reactor (NSRR), owned and operated by Japan Atomic Energy Research Institute (JAERI). In this test series, 4 segmented rods were subjected individually to pulse irradiation simulating an RIA, and a fuel failure was observed in the first test (HBO-1) where a peak fuel enthalpy of approximately 73 cal/g-fuel was assumed to be attained. The fuel failure occurred when the peak fuel enthalpy reached about 60 cal/g-fuel which is lower than the provisional failure threshold for burnup fuel (85 cal/g-fuel) in the current Japanese safety evaluation guideline on RIAs. This report describes a postulated mechanism on the failure observed in this test applying knowledge from transient data and post-irradiation examination data (1). The research program related to the failure of high burnup fuels under RIA conditions are also presented.

2. Characteristics of the fuel failure observed in the HBO-1 test

One of the important characteristics of the fuel failure observed in the HBO-1 test is cladding failure at a lower fuel enthalpy with very long axial cracks as observed in the former SPERT experiment with the rod preirradiated up to a fuel burnup of approximately 32 MWd/kgU. Figure 1 shows the post-test appearance of the test fuel rod. A long axial crack runs almost whole length of the fuel stack in a circumferential direction and it connected with a circumferential crack near the bottom end fitting weld which separated the fuel rod. Two separated long axial cracks also run on the opposite side of the cladding tube. The upper one extends to the top end fitting weld and runs along it. However, it did not separate the fuel rod there. The test fuel rod showed significant bending. At the observation of the appearance during the post-test examinations (PTEs), a possibility of
mechanical interaction was suggested between the sensor tip for the cladding axial elongation measurement and the body of linear variable differential transformer (LVDT) to cause this kind of bending. All of the fuel pellets was recovered from the bottom of the test capsule as relatively fine particles.

Figure 2 gives some of the transient records measured during the early stage of the HBO-1 test. The axial displacement of the top position of the fuel stack gradually decreased down to the lower detection limit showing a possibility of the loss of fuel pellets from the rod. The axial displacement of the top position of the cladding tube increased up to the upper detection limit showing a possibility of the separation of the fuel rod because this axial displacement is too much larger than that expected from the pellet-cladding mechanical interaction (PCMI). The thermocouples on the cladding surface failed at the very early stage of the transient when the cladding temperature was still very low (approximately 50°C). These transient data suggested a possibility of fuel failure and it was confirmed through the PTEs.

The time of the failure was identified from the transient records given in Fig.3 where the signals for cladding surface temperature, rod internal pressure and capsule internal pressure showed sudden changes at the same time. In particular, the signal for capsule internal pressure clearly shows a pressure generation within the capsule due to the fuel failure. This time was used to estimate the energy deposition in the rod at failure.

3. Some important knowledge from the PTEs for the HBO-1 rod
(1) Observation of the cross section of the cladding tube

Figure 4 shows a cross section of the cladding tube where a axial crack penetrates the cladding. Some characteristics can be pointed out from the figure such as extended hydride deposition and oval deformation. Residual cladding hoop strain estimated at a failed axial position was approximately 2%. This residual deformation of the cladding tube is much larger than that observed in the former fresh fuel rod tests reflecting the influences of the progress of fuel burnup such as severe creepdown of cladding tube and larger transient swelling of the fuel pellet probably due to the existence of large amount of gaseous fission products (FPs) in the pellets. Figure 5 gives the magnified view of a failed position. The figure shows severe hydride deposition near the cladding surface below the oxide film and generation of many small cracks vertical to the surface. This kind of cracks were found in many circumferential locations. Wall-through cracks were originated from some of these crack tips and showed a feature of ductile fracture in the inner cladding region. The average hydrogen absorption in the cladding was measured to be approximately 190 ppm.
(2) Circumferential cracks near the welds of end fittings

Some of axial cracks extended toward the welds of end fittings and run along them where heat affected zones existed. Additional deformation of the cladding tube similar to axial collapse was observed near the weld of top end fitting. These observations together with the bending of the fuel rod and oval deformation of the cladding tube suggested influences of axial constraint during the transient on the fuel failure which might be caused by the interaction between sensor tip for the cladding axial elongation measurement and the body of LVDT (see Fig.6). However, no visible damage was identified during the PTEs on the inner surface of the sensor body coated by polycarbonate.

4. Discussions
4.1 Failure mechanisms
(1) Influences of hydride depositions in the cladding tube

As stated before, the time of fuel failure was confirmed at around the end of power burst and before the rapid cladding temperature rise due to departure from nucleate boiling (DNB) on the cladding surface (the energy deposition at failure was estimated to be approximately 60 cal/g). This fact together with the appearances of the cracks shown in Fig.5 suggested the fuel failure due to PCMI at the early stage of transient assisted by the existence of the hydride depositions near the cladding surface because similar failure mode has been identified in the tests with the rods (JM rods) preirradiated in the Japan Materials Testing Reactor (JMTR)(2). Hydride depositions on the cladding surface of the JM rod were formed during the preirradiation and scattered on the cladding surface as small spots with diameters of usually less than 1 mm as shown in Fig.7. In the figure, a crack vertical to the cladding surface can be seen in the hydride deposition. Typical wall-through crack was originated from the tip of the crack in the hydride deposition and extended to the inner surface of the cladding tube showing a feature of ductile fracture as shown in Fig.8. The time of the generation of this wall-through crack was identified by the pressure generation in the test capsule and it occurred at the very early stage of the transient when the cladding temperature was still in low temperature. This situation is quite similar to that observed in the HBO-1 test.

To investigate the failure mechanisms of the HBO-1 rod, an additional pulse irradiation experiment (HBO-3) was performed with the rod with the same amount of fuel burnup but with a different level of oxidation on the cladding surface (that is, a different level of hydride depositions in the cladding). Sampling positions of the test rods used in the HBO-1 and HBO-3 tests are given in Fig.9 and numbered as NS-1 and NS-3, respectively (the test rods refabricated from the NS-2 and NS-4 positions were used in the HBO-2 and HBO-4 tests, respectively). Reference samples were taken from each position (NS-1 through NS-4) and subjected to examinations to identify the natures of the cladding tube and fuel pellet before pulse irradiation. Figure 10 shows hydride depositions in the
reference cladding tube samples. Hydride depositions are significant in the samples taken from the NS-1 and NS-2 positions and not significant in the sample taken from the NS-4 position. The sample taken from the NS-3 position has an intermediate condition. Average hydrogen concentrations in the reference cladding tube samples were measured to be 187, 152, 148 and 89 ppm at the NS-1, -2, -3 and -4 positions, respectively. This axial distribution of the hydrogen concentration in the cladding reflect the axial cladding temperature distribution during the burnup in the power plant. Figure 11 presents the results of the ring tensile tests performed at room temperature condition with the reference cladding tube samples. The sample taken from the NS-4 position (sample No. HBO4) with the lowest hydrogen concentration showed the most ductile behavior with the largest elongation. The other samples showed more embrittled behavior and the total elongation decreased with the increase of the hydrogen concentration.

The HBO-3 rod was tested under the almost same conditions as those in the HBO-1 test and the peak fuel enthalpy reached 74 cal/g (that in the HBO-1 test was 73 cal/g). The HBO-3 rod showed approximately 1.5 % of residual cladding hoop strain but did not fail. This residual deformation of the cladding tube is comparable to that estimated for the HBO-1 rod (approximately 2 %) and well correlated with data obtained from other PWR rod tests with lower fuel burnups where no fuel failure occurred as shown in Fig.12. The results of the ring tensile tests performed for the reference cladding samples corresponding to the HBO-1 and HBO-3 rods showed an influence of hydrogen concentration on the deformation behavior but it was not so significant. The only clear difference in two samples was the condition of hydride depositions near the cladding surface. These facts show that the PCMI-type failure observed in the HBO-1 test was a quite delicate process.

From the above-mentioned discussion, it can be pointed out that at least the initiation of the wall-through crack generation in the HBO-1 test is due to PCMI and the local hydride depositions near the cladding surface had an important influence on it. It should be also noted that the process of hydrogen absorption in the cladding tube is quite complicate and heavily depends on many factors other than fuel burnup as shown in the results of hydrogen analysis for the samples taken from the same long-sized rod for the HBO test series. In fact, the rod used in the HBO test series had a conventional design (for the assembly whose discharge burnup limit is 39 MWd/kgU) and was irradiated for 4cycles (3 cycles + an additional cycle) to accumulate desired fuel burnup for the purpose of demonstrating the performance of fuel at the current discharge burnup limitation of 48 MWd/kgU in assembly average. This situation is the same as that in the rod used for the first RIA test in the CABRI reactor (Rep Na1)(3). Longer residence of a fuel rod in the core may bring more water-side corrosion and thus larger hydrogen absorption. Contents of some impurities such as Sn or Nb in Zircaloy will strongly affect on the corrosion behavior and hydrogen absorption. Cladding heat treatment will be another factor to be considered in this problem. The hypothesized failure mechanism for the high burnup PWR rod under a simulated RIA conditions which was derived from the results obtained in the HBO test
series should, therefore, be confirmed by additional in-pile tests in the NSRR for the test rods with various level of water-side corrosion and different rod designs and by the out-of-pile tests on the separate effects relating to this kind of fuel failure.

(2) Influences of the axial constraint during the transient

As stated in Chapter 3, there are some physical observations such as bending of the test rod and oval deformation of the cladding tube which indicate the influences of the axial constraint on the behavior of the HBO-1 rod. If this axial constraint force could have an influence on the initiation of the wall-through crack generation, circumferential cracks should be observed in the hydride deposition zone near the cladding surface due to a bending stress. There is no such an indication. From the transient records, it is found that the cladding failure was initiated when the elongations of the fuel stack and cladding tube just started. Significant changes in elongation behavior occurred some time after the initiation of the failure. This suggests that the axial constraint had no major influence on the initiation of cladding failure. However, it might have an influence on enlarging the openings of the axial cracks and thus extending them to end fitting welds to cause a rod separation at the weld of bottom end fitting.

4.2 Consequences of the failure observed in the HBO-1 test

Mechanical energy generation during an RIA and its dependency on the fuel burnup are quite important research items in the NSRR program. As described in Chapter 2, all of the fuel pellets in the rod tested in the HBO-1 test was recovered from the test capsule as relatively fine particles. This suggests that a fuel pellet with a high burnup can be fragmented easily and largely into fine particles due to still undetermined phenomenon (or phenomena). Extensive grain boundary separation caused by the rapid thermal expansion of gaseous FPs existed and retained there might be one of the possible phenomena. Smaller average grain size observed in the fuel pellets with burnup of 50 MWd/kgU (see Fig.13) may be another important fact on this matter.

Large openings of the cladding tube and large fission gas release rate (FGR) observed in the HBO-1 test might have led some amount of direct fuel ejection from the openings. However, any significant pressure generation was not observed in this test. This may suggest that essential part of the fuel escaped from the position just above the bottom end fitting weld where a separation of the rod took place well after the transient and possibly during transportation of the capsule.

Quite high FGR during the transient (more than 20 % at lower fuel enthalpy) is another important finding in the high burnup PWR fuel tests. Indeed, FGRs measured in the HBO test series are much higher than those observed in other PWR rod tests where the
fuel burnups of tested rods are approximately 40 MWd/kgU. The difference in fuel burnup of about 10 MWd/kgU brought a big difference in FGR. This can be attributed to the formation of rim zone in the high burnup PWR fuel.

5. Related research programs in JAERI

In-pile RIA experiments for the rods with higher burnup and advanced design have the highest priority in the current and future NSRR program. As shown in Fig. 14, a new test series will start in this fiscal year with the test fuel rods refabricated from the rod irradiated together with the rod used for the HBO test series having different design as well as a new series with preirradiated MOX fuels. Another test series with the test fuel rods having an improved design for the current usage will be started in the next fiscal year as well as a new series with BWR rods of advanced design and higher burnups. Other projects in the NSRR program related to the behavior of high burnup fuel under RIA conditions are as follows;

① fresh fuel rod tests with uniform or localized artificial hydrogen concentration in the cladding tube
② separate effect tests including out-of-pile tests as requested and
③ analyses of the experimental results by the FRAP-T6 and FRAPCON-3 codes introduced from the USNRC and their validation.

Through these activities, remaining ambiguities on the transient behavior of high burnup fuels under RIA conditions such as influences of local hydride depositions will be clarified and a database for the safety assessment of burnup extension will be established.

6. Summary

From the experimental results obtained so far, it can be concluded that at least the initiation of the wall-through crack generation in the HBO-1 test is due to PCMI and the local hydride depositions near the cladding surface had an important influence on it. Axial constraint due to experimental arrangement might have an influence on enlarging the openings of the axial cracks and thus extending them to end fitting welds to cause a rod separation at the weld of bottom end fitting. The hypothesized failure mechanism for high burnup PWR rod under a simulated RIA conditions which was derived from the results obtained in the HBO test series should be confirmed by additional in-pile tests for the rods with various level of water-side corrosion and different rod designs and by the out-of-pile tests on the separate effects relating to this kind of fuel failure. Such research activities will be extensively continued in the NSRR program at JAERI together with international research cooperations.
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Fig. 1 Post-test appearance of the test fuel rod in the HBO-1 test
Figure 2: Transient records measured in the HHO-1 test.
Fig. 3 Identification of the failure time in the HBO test
Fig. 4  Cross section of the failed cladding tube in the HBO-1 test
Fig. 5  Magnified view of the failed position in the HBO-1 test
Fig. 6 Instrumentation system in the HBO-1 test
Cracking through Local Hydride Layer of Cladding Outer Edge in Test JM-5

Fig. 7 Hydride spot observed in the JM rod

Cladding Defect in Test JM-4

Fig. 8 Failure mode observed in the JM rod
Fuel Sample for the NSRR Experiment (NS-1-NS-4 Rod)

*: to be subjected to fuel examination as a reference sample

Test Fuel Rod (HBO-1~HBO-4)

Fig. 9 Test rod preparation scheme for the HBO test series
Fig. 10 Hydride deposition observed in the reference samples
Fig. 11  Results of the ring tensile tests for the reference samples

Fig. 12  Residual cladding hoop strains observed in the PWR rod tests
Fig. 13  Grain size changes in the PWR fuels due to burnup
Fig. 14 Long-term experimental program in the NSRR
The experimental test programme for the study of high burn-up PWR rods under RIA conditions in the CABRI core

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ABSTRACT

The CABRI facility mainly consists of an experimental reactor cooled with water, with a cell in its center which is the triple part of a sodium loop. This configuration allows to introduce in this cell an instrumented device holding a test rod. By this way, the rod is neutronically coupled with the core but thermohydraulically independent of it (as cooled by the sodium).

The CABRI core was extensively used for fast breeder fuel but, as it offers the feasibility of rapid and energetic power pulses, tests are now also performed for the study of the high burn-up PWR fuel rods under RIA conditions.

The instrumentation of the test section in which the test rod is introduced includes flowmeters, pressure transducers, microphones, thermocouples and a clad elongation transducer providing the informations required for the analysis of the events during and after the test. The geometry of the CABRI core also enables the use of a neutron detection system, the CABRI hodoscope, measuring the axial power profile in the test rod and giving indications on test fuel movements.

The test campaign itself lasting about two weeks, consists in :
- isothermal tests for the checking of the instrumentation,
- a so-called « heat balance test » (detailed hereafter),
- the transient itself,
- a core power plateau (at very low level) to get the final state of the rod as seen by the hodoscope.

The energy release in the test fuel is deduced from the measured energy deposition in the core and from the coupling factor between the test rod and the core, determined prior to the test itself by a sodium heat balance in the test channel during a core power plateau.

Non destructive post examinations and pre-examinations consisting in X-rays, γ-scanning and neutron radiographies are performed on the facility. This allows the comparison between the initial and final states of the test rod and eases the definition of the destructive examinations (metallography work) performed in the hot cells after the test.

The test programme includes up to now nine tests, among which four have been performed in the frame of a sharp pulse (9.5 ms of half height width).

The first test rod (REP Na1 test) was taken from a commercial fuel rod irradiated in an EdF power plant refabricated by the Fabric procedure leading to a maximum burn-up of 63.8 GWd/tU for the refabricated test rod. A failure of the test rod, detected by various transducers, occurred for an enthalpy of 30 cal/g and the post-test examinations showed multiple failures on almost the whole fissile length of the rod and fuel dispersion in the test channel surrounding the rod.

The test REP Na2 used a full length rod irradiated in the Belgian reactor BR3 with a maximum burn-up of 33 GWd/tU. No failure occurred during the test, but the metrology of the rod after test showed a significant radial deformation of the clad which confirms the indications of deformation noticed during the test (non reversible fuel and clad elongations, fast coolant flowrate expulsions, decrease of the residual flowrate).

The REP Na3 test rod (segmented fuel rod from an EdF commercial power plant with a maximum burn-up of 52.8 GWd/tU) led to a non-failure test, but the phenomena observed during the
test are similar to REP Na2, suggesting a rod deformation which is confirmed by the non destructive examinations.

Lastly, the REP Na5 test was performed on an EdF rod refabricated by the FABRICE procedure (with a maximum burn-up of 84 GWJ/IU and a weak corrosion); no rupture was observed and only a slight rod deformation can be deduced from experimental signals.

The orientations for the following tests of the programme are:
- tests on UO2 fuel rods with a different power pulse and a different state of clad corrosion (REP Na4).
- tests on U-Pu fuel (MOX)
I. INTRODUCTION

The design basis accident of control rod ejection in Light Water Reactors is considered with a high priority in the safety studies. This reactivity initiated accident leads to a fast power transient (some milliseconds to 1s) in conditions of reactor start-up (zero initial power). Criteria have been established based on SPERT experiments [1] using fresh and low burn-up irradiated fuel (with only two tests at 30 Gwd/t), but in the range of highly irradiated fuel, experimental basis and knowledge were still lacking and new safety evaluations were needed to check the validity of the criteria presently used for irradiated fuel and to evaluate the margin of these criteria for high burn-up.

With this aim, an experimental programme was defined in the CABRI reactor to extend the knowledge on the behaviour of irradiated fuel submitted to a RIA, with emphasis for the first step on the study of the early phase of a RIA transient up to rod failure.

Taking into account the specificity of the CABRI core, which is broadly developed in this paper (§II and II), the representativity of the tests compared to PWR reactor case has been evaluated by a calculation [2], [3]. It showed that the conditions of possible rod failure in the first phase of fast transient are well simulated by the tests defined in the sodium loop of CABRI.

Up to now, four tests were already performed on pins with different burn-up and level of clad corrosion. The tests conditions and the experimental results are developed in § V.

II. THE CABRI FACILITY (fig1)

II.1 CABRI driver core

II.1.1 Core geometry and fuel characteristics

The CABRI reactor consists in a driver core in a water pool, with fuel rods arranged by squared arrays cooled by water in upward forced convection inside a parallelepipedic « waterbox ». This core supplies thermal neutrons to obtain the required power in the test rod, located in a central loop with sodium flowing. It is constituted by 1488 rods of 6% enriched UO2 fuel and stainless steel cladding similar to those used in Pressurized Water Reactors. The fissile length of each rod is 80cm and the fuel pellet diameter is 8.85mm. Fifty-six borated aluminium absorbers are loaded in the corner regions.

Six control rods elements constituted with 23 neutron absorber hafnium rods are able to move in Zircaloy guide tubes. They allow to control the reactor, to keep it at a given power level and to shutdown the core power when dropped.

The CABRI core inside its « water box » is contained in an open-top cylindrical vessel of 5m in diameter and 10.5m high whose functions are to:
- confine stagnant water surrounding the core,
- support internal structures and equipment,
- form a containment barrier between the fuel and the surroundings.

The water in the pool is not directly used for core cooling except during natural convection core cooling, when reactor power is less than 100kW. At higher power levels, core cooling is achieved by forced circulation of water drawn from two 250m³ tanks at a nominal flowrate of 3200 m³/h.

II.1.2 Transient rod system (fig 2)

The Cabri facility has been designed to induce extremely short power transients. They are performed by a controlled reactivity injection obtained by the fast depressurization of four previously pressurized rods called transient rods.

The transient rod system is constituted by an in-pile part and an out-of-pile part. The four transient rods are the in-pile part of this system, each with 24 Zircaloy tubes normally placed under vacuum and filled with pressurized ³²He gas prior to each test. They are connected through collectors and fast opening valves to a 1m³ tank, located near the reactor.

The out-of-pile part of the system includes a compressor, a vacuum pump, a buffer and a helium storage tank.
The system is equipped by a « sequencer » which programs the various transient parameters: opening of valves, combination of valves, reactor scram by control rod drop.

II.2 Test loop

The sodium test loop consists of an out-of-pile part and an in-pile test cell, which receives the test section.

II.2.1 Out-of-pile

The out-of-pile part of the loop mainly consists of the pump, storage tank and piping for the sodium circulation and storage. Its basic functions are to:
- establish nominal conditions around the test pin,
- remove heat from the overall test cell during normal and accidental conditions
- confine radioactive products

II.2.2 In-pile test cell and test section

The in-pile part of the test cell (fig 3) is constituted by the part of the sodium circuits entering into the inside of the CABRI core all along its vertical axis. It is fixed and receives the experimental test section and the containment structure. The test cell is designed for the following functions:
- support the instrumented test sections where the test rods are introduced,
- position the test pin correctly with respect to the driver core and hodoscope,
- bring the test pin to the nominal thermohydraulic conditions required for the test,
- ensure the containment and protect the driver core against phenomena that occur within the test cell during test transients,
- allow insertion and withdrawal of the tests sections.

The experimental test section (fig 4) contains the test rod and the instrumentation and is replaced after each test. The containment structure is designed to insure sodium coolant by-pass flow around the test section and to contain fuel particles released during tests with pin failure.

II.3 Instrumentation

II.3.1 Driver core power measurements

The core power during the transient is determined with boron ionization chambers located in the pool of the core (fig 5). Two of them (chambers G3-1-1 and G3-1-2) having a good functioning up to about $10^5$ MW are used to follow the evolution of power for the REP Na tests. The consistency of this chambers is checked before each test using a heat balance method by measurement of the core water coolant temperature rise and mass flow (see III.2).

Previous tests on the CABRI reactor have shown that:
- a light non linearity of the signal of these chambers has been observed for high levels of the power core,
- a delay is introduced between the power traces given by these detectors (due to their position in the pool) and detectors located in the core specially for these previous tests.

These two points are taken into account by a correction of the signals of the chambers G3 before giving the final results for power and energy in the core.

II.3.2 Flowrate measurements

The flowrate measurements in the CABRI test sections have two purposes:
- determine the flowrate in the test channel under stationnary conditions (for the thermal heat balances, III.2)
- evaluate the flow variations due to the events occurring during the experiment

Two flowmeters constituted by two pairs of electrodes are devoted to the measurement of the flowrate at the inlet of the channel of the test section (F1; fig 4) and at the outlet (F2; fig 4). Each flowmeter is individually calibrated in an out-of-pile loop at different temperatures with a precision of $\pm 1\%$. During the isothermal tests (III.1), before the test itself, the coherency between the flowmeters F1 and F2 is checked. Their time response is less than 40μs.

The two pairs of electrodes for each flowmeter separated by 10 mm allow the detection of the passing of bubbles behind the flowmeters during gas release or voiding of the channel (the variation observed on the signal due to the passing of the interface is delayed from the « upstream» to the « downstream » electrodes).
11.3.3 Pressure measurement
The pressure in the test channel is given by two pressure detectors (strain gauges systems): P2A in the bottom part of the test section and P3A in the top part (fig 4). They are placed in « circulating chambers » to be protected from the thermal shocks due to the sodium ejections occurring during the ruptures.
They are calibrated at different temperatures and their time response measured during the dynamical calibration is less than 100µs.
Their good functioning is checked before the loading of the test section in CABRI and during the isothermal tests (see II.1).

11.3.4 Temperature measurements
The test section is instrumented by thermocouples located at different height and azimuthal positions. The thermocouples specially devoted to the measurement of the temperature in the sodium channel are sustained by the « cage system » which is a special device introduced in the sodium channel in order to hold the pin in position.
The thermocouples are used to perform the heat balance of the sodium in the channel (see II.2.3 ) and to check that the required conditions are obtained before the transient. Their variations give informations on the events occurring during the test and are used to determine the possible voiding zone.
Their time response is 40ms, so their signals has to be processed before analysis.

11.3.5 Acoustic measurements
They are used for the accurate timing of the pin failure. Two microphones located respectively in the lower part of the test section (M1) and in the upper part (M2) are used to detect the acoustic events attached to the events occurring during the tests. These acoustics events may be produced by mechanical shocks, bubble collapse during the boiling phase, clad rupture, fuel-clad interaction events and fission gas release. As a consequence, the signals of the microphones show an increase compared to their normal background. A clear relation between an observed signal increase and the origin of this signal can be made knowing the transmission path for acoustic waves; this allows to determine the location and time of the rupture. The time response of the microphones is negligible (less than 5µs).

11.3.6 Two-phase flow detectors
Three void detectors are available in each test section: VD1 below the inlet of the test channel and two other ones VD2 and VD3 above the outlet at two different positions. They only give qualitative informations for the detection of the passing of bubbles and to built the voiding zone.

11.3.7 Displacement transducer
A displacement detector is placed on the nose of the pin. Taking the position of the pin at the beginning of the test (at isothermal conditions) as the reference position, the variations of this detector are analysed quantitatively in terms of pin elongation using the previous calibration of this detector.

11.4. Hodoscope (fig 6)
The axial power profile of the pin in the CABRI core is measured by the Hodoscope which observes the pin by measuring its neutron fission emission as a function of time. By this way, this device allows to follow in line the fuel motion during the power transient (elongation, radial deformation and axial dispersion).
The driver core is separated into two parts by a wide air-filled channel which passes through the center of the core. The channel extends outside the core on one side through the reactor vessel wall to a collimator and a neutron detector system. The collimator is a 3m long mild steel prismatic block bored with three columns of 51 rectangular channels. At the rear of the reactor, each channel is equipped with two detectors, one behind the other (fission chambers and proton recoil detectors).
The collimator is adjusted before the test so that two rows observes the pin and the last one is devoted to the background measurement. Each channel receives the radiation emitted by a zone of 10mm wide by 20mm high in the vertical plane to the test pin axis and perpendicular to the collimator axis.
III. EXPERIMENTAL PROCEDURE

After the introduction of the test section in the test cell, preliminary tests are performed.

III.1 Isothermal tests

This set of tests allows to check the consistency of the whole instrumentation under isothermal conditions:
- consistency of the thermocouples under sodium flow circulating in the channel; their signals are individually corrected from the mean value,
- coherency of the flowmeters for different flowrates of the sodium,
- zero of the flowmeters by isolation of the sodium cell (stagnant sodium) and by a voiding of this cell (without sodium),
- good functioning of the pressure detector by varying the pressure in the Argon blanket of the cell.
- good functioning of the void detectors during the voiding of the channel.
Furthermore, it is checked that the hydraulic conditions required for the test are obtained.

III.2 Heat balances

They are performed just after the isothermal tests and before the test itself. The signals given by a selected part of the instrumentation in the core and in the test section are recorded during two power plateaus $P_I$ (intermediate) and $P_F$ (final).

III.2.1 Core heat balance: checking of the calibration of the power chambers

The informations given by the thermohydraulic instrumentation (core water coolant temperature rise and mass flow) during a power plateau are used for the determination of the core power at this plateau by a water heat balance.

A comparison is done:
- between the signals given by four power chambers at this plateau to check their consistency,
- between the signals of the chambers and the value obtained by the core heat balance, to check the calibration coefficients of the chambers.

III.2.2 Sodium heat balance: determination of the coupling factor of the test rod

The temperature rise of the sodium all along the fissile column and the nominal value of its flowrate in the channel during the plateau gives the rod fissile power by a sodium heat balance, taking into account the thermal losses and removing the contribution of gamma heating to the increase of temperature of the sodium.

The coupling factor, which is defined by the ratio of the core power to the fissile power of the rod, is then calculated. It is one of the parameters used in the calculation of the energy released in the pin during the test (see III.3).

III.3 Transient

The transients are performed from a core power plateau at 100kW, reproducing a sequence of depressurisation of the transient rods which is determined during previous test.

During the 100kW power plateau, the axial power profile (which depends on the control rod position) and the fissile length of the rod are given by the hodoscope.

After the test, a core power plateau at 100kW is performed to allow the recording of the final state of the rod by the hodoscope; this final state will be compared after the test with the initial state recorded on the power plateau just before the test.

As the hodoscope supplies the evolution of the fuel all along the test, the elongation of the fuel column can be followed and compared with the elongation of the pin given by the displacement detector.
III.3.1 Detection of events by the instrumentation

Part of the instrumentation of the test section is able to detect the events occurring during the transient:

- **Microphones:** Their signals are sensitive to acoustics events associated to rod failures and are used to determine the precise timing of the failure (see II.3.5).

- **Flowmeters:** A rupture is detected on these detectors by a strong variation of their signals corresponding to the flow disturbances created by this rupture. Consequently, the onset of these variations associated to a propagation model can give a localisation of the rupture. In the disturbed regime following the rupture, the difference of the integrals of the two signals of flowrate versus time is equal to the volume of gas which is release; so, it contributes to the estimation of the voiding zone. Another information for the determination of the voiding zone is the passing of bubbles at the level of the flowmeters; as each flowmeter has two electrodes, the disturbances created on the signals by the passing of bubbles are observed with some delay between the two electrodes.

- **Pressure detectors:** All the events, such as gas release and fuel ejection leading to pressure variations in the test channel can induce pressure peaks observed on the signals of the two pressure detectors. These pressure peaks propagating in the sodium with the velocity of compression waves, can be used for a localisation of the rupture.

- **Void detectors:** As described in II.3.6, they detect the passing of gas released in the test channel at their positions (fission products or sodium vapor) and give in this way a contribution to the limits of the voiding zone.

- **Thermocouples:** Very strong or fast variations of temperature are observed on the thermocouples located in the channel which are an indication of fuel ejection, sodium boiling or passing of an interface sodium/gas. They can be interpreted and used (after signal process; see II.3.4) in the building of the voiding zone.

III.3.2 Energy released in the test rod during the test

The energy released at peak power node versus time in the test rod by mass unit during a transient is given by:

\[
\xi(t) = \frac{E(t) \times FF}{M \times C}
\]

where:

- \(E(t)\) is the energy in the core at t (from the integral for the power of the core given by the signals of the power chambers),
- \(M\) the mass of the test rod,
- \(FF\) the axial form factor given by the hodoscope,
- \(C\) the coupling factor of the pin obtained as described in II.2.2

This evaluation leads to determine the energy release in the test rod at each time during the transient within ±6% uncertainty. The time of rupture being determined by analysing the « time of flight » of the acoustic waves detected by the microphones, the energy release at the time of rupture is then deduced.

IV. NON DESTRUCTIVE EXAMINATIONS

**IV.1 Pre-test examinations**

Before the test, non destructive examinations are performed in SURA facility on the rod, after its introduction in the test section. X-ray radiographies and gamma scannings gives the initial state of the fuel and supplies informations on axial power distribution along the intact rod.

**IV.2 Post-test examinations**

After each test, the test section is removed from the test loop to undergo post test examinations. X-ray radiographies and gamma scannings are performed in order to obtain the final state of the fuel, to be compared with the initial state of the rod. Neutron radiography is performed with a beam of thermal neutrons from the SCARABEE reactor, providing qualitative informations on the location of the fuel. The test rod is then sent to the hot cells destructive examinations.
V. RESULTS OF THE TESTS

V.1 REP Na1

V.1.1 The refabricated fuel rod

The rod chosen for the first test of the programme was refabricated by the FABRICE process (DRN/DMT/SETIC/LECR). This technique is used to produce short rods in hot cells from long power reactor rods, so it makes available pre-irradiated rods which can be tested in experimental reactors. There are six major stages in the refabrication process:

- selection and characterisation of the irradiated fuel element,
- cutting out of the section,
- removal of the oxide from the ends for attaching the end-plugs and springs,
- sealing under controlled atmosphere,
- quality control of fabrication.

The test rod was refabricated from an initial rod (4.5% enriched in U5) irradiated during 5 cycles in the EDF GRAVELINES reactor (rod QO2 from the fuel assembly FFOLTWJV). The fissile column of the FABRICE REP Na1 rod was taken from a section of the initial rod so that the fissile column was centered on the intergrid fuel assembly 5-6 (in the upper part of the initial rod); it corresponds to a section located from 2253 to 2822 mm of the bottom of the initial rod, giving a fissile length of 569 mm.

The maximum burn-up of the refabricated rod issued from EDF calculations leads to 63.8 GWd/tU. The measurement of the layer of Zirconium on the initial rod showed thicknesses up to a mean value of 80 μm in the region with the most oxidized zones with indications of strong desquamation.

V.1.2 Conditions of the test

The test REP Na1 was performed on November 10th, 1993. The transient (fig 7) was initiated from a steady-state plateau with a core power of 100 kW (which correspond to few watts in the test pin) and sodium flowing in the channel at 279°C with a flowrate corresponding to 3.8 m/s.

The main characteristics of the transient are the following ones (fig 7):

- Maximum value of the core power : 18407 MW
- Half width : 9.5 ms

Heat water and sodium balances have been performed before the test for the checking of the coherency of the power chambers and for the determination of the coupling factor. The conditions of realisation of these previous tests were defined so that the initial state of the rod was not disturbed before the transient.

V.1.3 Results

• Determination of the rupture

An early rupture is detected; the first indication is shown by the microphones signals variations, immediately followed by the flowmeters and pressure detectors. The set of techniques usually used for the correlation of these different signals to determine the location and time of rupture do not allow in this case a precise determination of the failure. In fact, this set of signals seems to be representative of a very fast succession of events occurring at different locations of the fissile column. So, the time of rupture was calculated on the basis of detection by the microphones. The times of rupture given by the others detectors allow to define a time interval including the successive failures.

• Evaluation of the energy release

The energy release in the rod all along the transient is calculated using the evolution of the core power versus time (fig 7) and the formula given in II.3.2. This lead to a maximum energy release in the rod during the transient of 110 cal/g (0.4s after the beginning of the transient).

As this test was characterized by an early rupture, the level of the energy release in the rod at the time of rupture was very low level going from:

• 11.7 cal/g for the time of rupture obtained with the microphones detection,
• 19.7 cal/g for the time of detection on the flowmeters.
V.2 REP Na2

V.2.1 The BR3 rod

The rod chosen for the test REP Na2 was n° U 641 Lot n° 4-1 issued from the reactor BR3. Its characteristics were the following ones:

- Maximum value of the burn-up: 33 Gwd/t
- Enrichment: 6.85% in U5
- Length of the rod: 1m
- Thickness of the layer of Zirconium: 10um

V.2.2 Conditions of the test

The test was performed on June 10th, 1994 with the conditions similar to the test REP Na1. The transient was triggered from a 100kW steady-state plateau and led to:

- Maximum value of the core power: 17875 MW
- Half width: 9.5 ms

At the steady-state before the test, the sodium was flowing in the channel with a flowrate corresponding to 4m/s at 280°C and, as in REP Na1, heat water and sodium balances have been performed before the test to obtain the data necessary for the analysis after test.

V.2.3 Results

No rupture occurred in this test. Due to the fact that the coupling factor in CABRI is better in the case of the BR3 rod than it was for the rod used in REP Na1, the maximum energy released in the rod during the test REP Na2 is 211 cal/g (at 0.4s after the beginning of the transient).

During the TOP, a strong elongation of the fuel column was measured by the hodoscope during 10ms with a final elongation of 8mm; around the same time, the displacement detector indicates the onset of lifting of the clad which stabilizes at 11mm. A residual elongation of 4mm of the fissile column is measured by the hodoscope after test and confirmed by the non-destructive examinations.

Less than 5ms after the onset of this phenomenon, a variation of the signals of the two flowmeters (about 30% of the nominal value) is detected which can be interpreted as a swelling of the clad of the test rod due to pressure exerted by the fuel in contact with the clad.

Lastly, a weak decrease of the flowrate in the channel (4% of the nominal value) was observed after the test. It corresponds to a residual swelling of the rod which was confirmed by a metrology of the rod (measurement of the mean deformation of about 3%).

V.3. REP Na3

V.3.1 The segmented rod

The test rod was constituted from a segment cut off (in the intergrid location 5-6) from an initial segmented rod (J12 from the FFOLCLJV assembly; enrichment 4.5% U5) irradiated during 4 cycles in the EDF GRAVEINES reactor. The final segment had a fissile length of 463mm and is equipped with seal welded tips at the extremities. The maximum value of the burn-up of the test rod is 52.8 GWD/tU. The maximal value of the thickness of the Zirconium layer is 40um.

V.3.2 Conditions of the test

As usual, heat water and sodium balances were performed before the test. The test itself, on October 6th, 1994, started with a steady-state plateau at 100kW of core power with sodium flowing in the channel with a velocity of 4m/s at 280°C.
The TOP, with identical conditions of REP Na1 and 3, reached a maximal core power of 18900MW with a half width of 9.5ms.

V.3.3 Results
No rupture was detected by the instrumentation during the test. The maximum energy released in the rod (at 0.4s after the beginning of the transient) is 120 cal/g. The only detectors showing significant variations of their signals are the two flowmeters and the displacement detector which presented variations similar to those observed in REP Na2 where the flowmeters disturbances were attributed to a rod swelling. In the case of REP Na3, the metrology performed on the test rod showed a 2% maximal variation of the diameter of the rod due to the swelling.

V.4 REP Na5

The early rupture of the rod in REP Na1 and the non rupture in REP Na3 led to the need of a third test in the same conditions as REP NA1 test (test rod with a high burn-up level), but with a weaker level of corrosion of the clad.

V.4.1 The test rod
Taking into account the previous conditions, the test rod was refabricated by the FABRICE process (described in V.1.1) from an initial rod EDF1065 irradiated during 5 cycles in the GRAVELINES reactor (enrichment 4.5% in U5). The final test rod was taken from a section located on the intergrid 2-3 of the rod, on the lower part of the initial rod to obtain a feasible thickness of the Zirconium layer. The length of the fissile column of the test rod is 563 mm and the the thickness of Zirconium on the clad is about 20μm (standard cladding). The burn-up of the refabricated rod is evaluated to 64 GWd/tU by calculations.

V.4.2 Conditions of the test
The REP Na5 test was performed on May 5th, 1995, after the usual thermal balances. The conditions are nearer from the test REP Na1:

- Steady-state core power plateau : 100kW
- Temperature of sodium at steady-state : 280°C
- Velocity of sodium in the channel : 4m/s
- Maximum core power during the transient : 19500 MW
- Half width : 9ms

V.4.3 Results
No rupture occurred during this test. The maximum energy release in the rod is 105 cal/g (at 0.4s after the beginning of the transient). After the transient, the elongation of the rod given by the displacement detector is 6mm and the hodoscope detected a residual elongation of the fuel column of 2mm after the test (comparison between the initial and final states).
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Assessment of Light Water Reactor Fuel Damage during a Reactivity Initiated Accident

Irradiated fuel behaviour during reactivity initiated accidents in LWR's: Status of research and development studies in France
NT SEMAR 93/92

Precalcul de l'essai CABRI REP Na1 avec le code SCANAIR
NT SEMAR 94/11
FIGURES

Figure 1 - The CABRI experimental facility
Figure 2 - Transient rod circuit layout
Figure 3 - In-pile loop
Figure 4 - Scheme of the test section, by-pass and instrumentation
Figure 5 - Location of the core power detectors
Figure 6 - Hodoscope in the CABRI facility
Figure 7 - Power trace and energy during the transient in REP NA1
FIGURE 1 - THE CABRI EXPERIMENTAL FACILITY
FIGURE 2 - TRANSIENT ROD CIRCUIT LAY-OUT
Test Section:
Flowmeters F1 and F2
Pressure Transducers P1, P2 and P3
Void Detectors VD
Microphones M

By-pass:
Flowmeter F3
Pressure Transducer PC
Sodium Level Gauge NISP 66

FIGURE 4: Scheme of the Test Section, By-Pass and Instrumentation
Distances between core and power chambers:
- 2-1: 419mm
- 2-2: 339mm
- 3-1-1: 700mm
- 3-1-2: 600mm

GURE 5 - location of the core power detectors
VERTICAL AXIS

TEST PIN
SLOT IN THE CORE

REACTOR TANK
LOOP

SHIELDING

REACTOR CORE
TEST PIN
SLOT IN THE CORE

GUIDE + SUPPORT RAILS
RAIL FOR COLLIMATOR

COLLIMATOR

FIGURE 6: Hodoscope in the CABRI facility

DETECTOR HOLDERS
FIGURE 7- power trace and core energy during the transient in REP Na1
CLADDING and FUEL MODIFICATIONS of a 60 GWj/tM irradiated rod during a power transient performed in the CABRI-reactor

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Preamble

Economic criteria has led the EDF to increase the duration of fuel rods in pile, by making the refuelling in the Pressurised Water Reactors longer. In this context, fuel burn up after 5 cycles of irradiation reaches the value of about 60 GWd/tU.

The reactivity initiated accident (RIA) of the PWRs, which is the reference accident, is postulated to result from the ejection of a control rod bundle out of the core.

In France, RIA safety criteria have been formulated on the basis of the SPERT and PBF experiments [1].

- Maximum mean fuel enthalpy: \( H(\text{maxi}) < 200 \text{ cal/g} \)
- Maximum clad temperature: \( T_c(\text{maxi}) < 1482^\circ \text{C} \)
- Maximum fuel melt fraction: \( F(\text{melt}) < 10\% \)

and could be summarized by the following recommendations:

No rod failure without reaching departure from nucleated boiling
No fuel dispersion without melting

These criteria were mainly established with fresh fuel test results and were formulated independently of the burn-up, but it was clear, according to [1] that "the mode of the fuel rod failure is strongly affected by the previous irradiation".

Consequently, in 1993, the CEA - Institut de Protection et de Surete Nucléaire IPSN (French Nuclear Protection and Safety institute) and the EDF started an experimental program in the IPSN-Cabri test reactor.

That same year the first test, so called REP-Na 1 was performed and the program has continued up to now with 5 tests using \( \text{UO}_2 \) irradiated fuel. The following tests are scheduled for the end of this year and at the beginning of 1996 using irradiated MOX fuels.

Due to the early failure of the REP-Na 1 rod test, many post test examinations (both non destructive and destructive) were performed in CEA hot cell laboratories. Two main poles of interest were selected: modification of the cladding (cracking, hydrides, ...) and evolution of the fuel (porosity, microstructure, ...) in order to understand what happened during this test.
1. INTRODUCTION

The major objective of the REP-Na 1 test was to determine the enthalpy level at rod failure for a high burn-up UO₂ fuel and to estimate the level of fuel dispersion after the rupture. Some test results emerged before the post test examinations and they are fully described elsewhere [2]. In this test rod failure occurred at energy deposit value lower than expected and must be analyzed taking all the tests of this program into account. Our objective, in this present paper, is to describe the rod as completely as possible, before and after its ultimate evolution during the RIA test in the Cabri reactor.

2. GENERAL POINTS

2.1. Initial characteristics of the rod

The experimental rod chosen for this first test was irradiated in the EDF PWR GRAVELINES, for 5 cycles. A standard fuel made up of UO₂ enriched in U²³⁵ up to 4.5% was used. Due to the geometrical characteristics of the experimental Cabri reactor, a refabrication step was required. It is called "Fabrice process" [3] and it consists of a rebuilt section of the initial rod. The section used for REP Na 1 was initially located between level 2253 mm and 2822 mm starting from the bottom of the rod (namely the part of the fissile column centered on the inter-grid 5-6 of the assembly).

Burn-up of the refabricated rod came from EDF calculations and was about 63 GWd/tU.

Some examinations were performed near the part of the rod used for the rebuilding, in order to have an initial state of the fuel and the cladding prior to the Cabri test (Unfortunately, the clad observed here is not representative of the test cladding, due to the grid location). The main results of these first observations (figures 1 and 2) can be summarized as follows:

UO₂:

- A RIM effect is optically seen on the fuel (50 μm in thickness)
- A central zone of intergranular gas bubble precipitation, the diameter of which accounts for 46% of the pellet diameter.
- The central part is characterized by very thin intragranular bubbles

Clad:

- Zirconium oxide is partially present on the external part of the clad with a thickness of 80 μm (figure 2) with an important desquamation. At grid location the value is 40 to 50 μm.
- 300 ppm of hydrogen content in the clad at the grid location (circular hydrides).

Figure 1: Macroscopic examination:
optical view after etching of the initial rod [Q02 rod of FFOLTWJX assembly], performed at level 2212 mm/bottom of fissile column. 1 mm

Figure 2: Zirconia thickness (Eddy currents) on the Q02 father rod
2.2. Conditions of the Cabri test [4]

In case of a control rod ejection in pile, the initiated reactivity leads to a fast power transient under reactor start-up conditions (initial power: zero). Therefore, the ramp of the Cabri test was chosen to simulate this kind of transient. The transient was initiated from a steady state plateau with a core power of 100 kW and sodium flowing in the channel at 279°C with a flow rate corresponding to 3.8 m/s.

The characteristics of the transient are:
- Maximum value of the core power: 18407 MW
- Half width: 9.5 ms

2.3. First results of the test [4]

Clad rupture occurred very early. Through different means, the released energy measured at the moment of the failure is around 20 cal/g.

The hodoscope measurement showed a loss of fuel between 220 and 320 mm from the bottom of the fissile column. The fuel located in the filters was estimated (by gamma scanning) to be ~6 g.

Gammascan and neutron radiography were performed right after the test (figure 3) and show the important modifications that occurred in the fuel pellet column.

![Figure 3: gamma scanning of the Cabri test REP Na-1 performed at Cabri](image)

3. RESULTS OF THE DESTRUCTIVE EXAMINATIONS IN HOT CELLS

Upon arrival in the hot cell, the rod test was cut into three sections:
- from 500 mm/bfc beyond the upper plug
- from 150 mm/bfc above the lower plug
- from 500 mm/bfc to 150 mm/bfc

Only the central part required special treatment in order to eliminate the sodium and then it was strengthened and embedded with araldite. The two other parts were visually examined. They both present straight failures in the fuel parts (figure 4).

![Figure 4: Periscopic view of the cracking at a level beyond 500 mm/bfc 0.5 mm](image)
Many cross sections were metallographically examined all along the 3 sections in order to describe as fully as possible:
- the clad cracking (morphology of the cracks, association or not with the hydrides, with the zircaloy structure),
- the zirconia layer
- the hydride evolution (related to the clad deformation, to the cracking),
- the evolution of fuel (macroscopic effect: grain size, swelling, or microscopic effect: modification of the fission gas location, porosity size).

3.1. Clad Cracking

We propose here, a presentation of the fracture features. All along the presentation we must recall that the observation is a static picture corresponding to a final state which is not fully representative of what happened during the test itself. The cross section observation may make us think that the cracking has a radial propagation, and it is more realistic to imagine an axial one in connection with figure 4. It is important to note that very few external zirconia layers left after the test.

Two types of cracks are observed: straight or slashed. They are illustrated by these two figures (figures 5 and 6).

![Figure 5: Microscopic view of a slashed clad rupture (level 103 mm/bfc) ——— 100 µm](image1)

![Figure 6: Microscopic view of a straight clad rupture after etching (level 105 mm/bfc) ——— 100 µm](image2)

In addition to the main through wall failures, small cracks are observed in many locations. Most of them are opened towards the inner clad side (figure 7) but some are located in the outer periphery of the clad. This kind of crack is associated with the great hydride concentration zone which probably occurred before the test.

A mechanism explaining the presence of such zones could be the desquamation (flaking off) of the external zirconia layer observed after the irradiation in the PWR. Therefore the temperature of the clad decreases locally promoting hydride precipitation which makes the clad brittle.

![Figure 7: Microscopic view of two kinds of fissuring (inside or outside of the clad) (level 168.5 mm/bfc) 100 µm](image3)
This special kind of cracking (external) was clearly associated with an optically modified zone of the cladding. It was important, in connection with the test itself, to specify the chemical composition of these zones. So microprobe examinations were performed with the EPMA of the CEA/Saclay, at level 295 mm/bfc.

A microprobe analysis of 5 elements (Zr, O, Sn, Fe and Cr) was carried out along a radial crossing of the clad in an optical modified clad region. Figures 8, 9 and 10, show a decrease of Zr and O content in the external part in comparison with the internal one having no modifications of the Fe, Sn and Cr elements. That leads us to believe that a hydride phase is present in the external part.

Figure 8:
Content of Zr (% in weight)

Figure 9:
Content of Sn, O (% in weight)

Figure 10:
Content of Cr and Fe (% in weight)

The same analysis was performed around the cracking located on the inside part of the cladding (figure 7). All results are similar, indicating that failure borders are hydrogen rich. However the external parts of the clad may be hydrogen enriched due to zirconia desquamation in PWR whereas hydrogen located along the cracks must come from sodium, after failures occurred.
3.1.1. Hydrides

With the exception of the two regions examined in the previous paragraph (figure 7), hydrides in the clad have, generally, a common morphology (only the concentration is different from the initial one).

But:

around some clad failures they are influenced by the stress and appear with a modified repartition (figure 6).

in two different locations from the rod, hydrides appear with a very unusual radial orientation (figures 11 and 12).

The first one (figure 11) located at the 8 mm/bfc level does not seem to be related to the cracking. It could have occurred in the ultimate state of the test by a process unrelated to the test itself (thermal effect in a stress status).

At level 173 mm/bfc (in the middle of the test), (figure 12) radial hydrides are seen in the internal region of the clad. They could result from a fuel clad interaction.

Furthermore the global H₂ content was measured and related to the zirconia thickness (70-80 µm at this level).

The opposite figure which gives the correlation between the external zirconia thickness and the hydrogen content in a PWR clad, shows that the measurements performed on REP Na-1 and the Q02 father rod are in quite good agreement with results obtained so far.

3.2. Fuel

3.2.1. Pellet and RIM

The macroscopic examination (figure 13) performed at level 173 mm/bfc shows the fuel structure after the REP Na-1 test. A microscopic view of the fuel (figure 14a) performed at this level can be compared with the reference state using the Q02 rod (figure 14b).
Significant apparent swelling of the pellet is measured: around 5-10% (diametral increase). The fuel pellet is highly fragmented and this is quite obvious under the microscopic examination performed at level 173 mm/bfc (figure 14 a). A part of the RIM seems to be missing. A microprobe analysis of the fuel performed at level 295 mm/bfc shows that around 40 μm thickness (in comparison with the Pu content in weight % with a “standard” REP 5 cycles) has been removed (figure 15). The xenon content is unchanged (figure 16) in comparison with the same measurement performed on a 5 cycle fuel.

**Figure 13:**
Macrographic examination:
optical view after Cabri REP Na-1 test, performed at level 173 mm/bfc
1 mm

**Figure 14:**
Micrographic examination:
a/ optical view after Cabri REP Na-1 test, performed at level 173 mm/bfc
b/ optical view after etching of the Q02 rod before the Cabri test
10 μm

**Figure 15:**
Pu content (% in weight) in the REP Na-1 fuel after the Cabri test compared with the analysis of a standard GRAVELINES 5 cycles.
3.2.2. Fuel in coolant

Some particles, located inside the coolant are collected on the upper filter grid (opening φ is 40 μm). All the particles collected represent 4.09 g in weight and have been observed under optical microscopy. Numerous particles are probably pieces of zirconia coming from the external layer of the rod (figure 16a), only a few particles are fuel ones (figure 16b).

![Figure 16: Microscopic view of collected pieces on the upper filter grid.](30 μm)

3.2.3. Porosity

A secondary electronic microscopy (SEM) analysis was performed in Saclay hot cells. In order to approach the intragranular porosity, observation of a polished sample was carried out. Concerning the grain surfaces, an examination of a fractured sample was performed. Comparison with a 5 cycle fuel was accomplished.

3.2.3.1. SEM on polished sample

In the view presented in figure 17, the intragranular porosity appears with about 1 μm in large, as it is often observed in a 5 cycle fuel. The density pores number is very similar to the one of a 5 cycle fuel. That is well correlated with the xenon content shown in § 3.2.1

![Figure 17: SEM examination of a polished sample of the fuel of REP Na-1, at level 295 mm/bfc](20kV x2,000 10μm 278295)
3.2.3.2. SEM on fractured sample

In the following view (figure 18) the intergranular porosity is observed without damage due to the preparation. The important fact to notice is the presence of micro bubbles on the boundary surfaces. This fact is unusual in the 5 cycle fuel and it may be due to the Cabri test itself.

![SEM examination of a fractured sample of the fuel of REP Na-1, at level 295 mm/bfc](image)

Figure 18:
SEM examination of a fractured sample of the fuel of REP Na-1, at level 295 mm/bfc

4. CONCLUSIONS

According to the destructive examinations, it is possible to observe some evolution after the Cabri test REP Na-1, concerning the hydrides distribution in the clad, the fuel microstructure and fission gas behaviour.

Clad failure is considerable all along the rod. The macroscopic aspect of the cracks is brittle.

Hydride structure has been modified even if the global content appears to be the same.

Some pre-existing hydride rich zones located in the external part of the clad, due to zirconia desquamation, seem to be favourable for cracks initiation coming from the outside. This initiation is probably due to the pellet-clad interaction as a consequence of the fission gas bubbles increase in the intergranular location in the fuel pellet. Thus the cracking can move towards axial and straight fissuring. The dissolved xenon, as for it, have an unchanged concentration in the solid fuel.

Due to the clad failure, part of the fuel is released (at least 4 g) and the RIM is damaged (about 40 μm in thickness is not seen after the test), due to the high fragmentation.

References


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THE BEHAVIOUR OF IRRADIATED FUEL UNDER RIA TRANSIENTS: INTERPRETATION OF THE CABRI EXPERIMENTS.

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1. INTRODUCTION

The CABRI experimental programme has been designed in the frame of the safety studies related to the Reactivity Initiated Accidents and in view of the future burn-up increase in the French PWRs [1], [2]. It is performed under the collaboration between Nuclear Safety and Protection Institute (IPSN) and Electricité de France (EDF).

The aim of this programme is to investigate the behaviour of highly irradiated fuel under fast power transients and to provide knowledge on the physical phenomena for the development and validation of the computer code SCANAIR, elaborated at IPSN [3].

Up to the years 90s, the available data base (SPERT, PBF experiments) has been limited to fresh or low irradiated fuel (up to 30 GWd/t) and has served as a basis for the establishment of the safety criteria which are presently independent of burn-up level.

However, several aspects of the high irradiation level may affect the fuel rod behaviour under RIA transient such as:

- the important clad oxidation with possible spallation of the zirconia layer leading to clad embrittlement and reduction of the mechanical strength,
- the high fission gas retention which induces transient fuel swelling and thus strong pellet-clad mechanical interaction (PCMI),
- the presence of the "rim" zone with high porosity, high local burn-up and plutonium content, and small grains structure.

As a consequence, early rod failure followed by dispersion of finely fragmented fuel with fuel coolant interaction (FCI) might be expected.

A first analysis of the recent NSRR tests with irradiated fuel [4] clearly showed that due to PCMI, early rod failure occurs with cold clad: such mechanism appears different from the case of fresh fuel rods in which clad failure occurs lately after DNB at quenching.

The low influence of the clad-coolant heat transfer during the initial phase of the fast power transient allowed in a first step, to define experiments in the CABRI sodium loop with the objective of determination of the fuel enthalpy threshold for rod failure and onset of fuel ejection.
This experimental programme, still underway, consists of six tests with UO$_2$ rods and three tests using MOX fuel to be realised up to end of 96 (see matrix, table 1) with as main parameter the burn-up level from 30 to 65 GWD/t.

In this paper, we will concentrate on the analysis of the three first UO$_2$ tests REP Na1, REP Na2, REP Na3 with some elements on REP Na4 and REP Na5.

The interpretation of these tests is based on the experimental results including non-destructive and destructive examinations, as discussed in [5] and [6] and on the quantitative analysis performed with the SCANAIR code.

Evaluation of open issues and future tasks will be discussed.

II. DESCRIPTION OF THE CABRI REP Na TESTS

1) Tests conditions

The REP Na tests are performed in the sodium loop located in the centre of the CABRI reactor. The experimental procedure and the instrumentation for test diagnostic are discussed in [5].

The coolant channel conditions are the following:

- sodium inlet temperature : 280°C,
- sodium velocity : 4 m/s,
- coolant outlet pressure : 2 b

Except the pressure level, these thermal-hydraulic conditions simulate the PWR reactor hot-stand-by state.

The power transient is initiated from initial zero power and has a 9.5 ms half width : such rapid power pulse is consistent with the existing RIA data base (SPERT, NSRR tests). The energy deposition corresponds to the maximum power of the CABRI driver core.

In the future tests, power pulses with larger half width (25 to 60 ms), will be realised in order to better simulate reactor transients.

The axial power profile along the test rod is of cosine shape following the CABRI flux and the radial power profile depends on the self-shielding varying with the rod burn-up (maximum power in the periphery of the rod).

The main characteristics of the tests REP Na1 to 5 are summarised in the following table 2.
TABLE 2 : MAIN CHARACTERISTICS OF THE TESTS

<table>
<thead>
<tr>
<th>Test fuel rod fissile length (mm)</th>
<th>REP Na1</th>
<th>REP Na2</th>
<th>REP Na3</th>
<th>REP Na4</th>
<th>REP Na5</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>EDF/Fabrice</td>
<td>BR3 rod</td>
<td>EDF/segmented</td>
<td>EDF/Fabrice</td>
<td>EDF/Fabrice</td>
</tr>
<tr>
<td></td>
<td>569</td>
<td>1000</td>
<td>440</td>
<td>571</td>
<td>571</td>
</tr>
<tr>
<td>Cladding</td>
<td>standard</td>
<td>standard</td>
<td>improved</td>
<td>standard</td>
<td>standard</td>
</tr>
<tr>
<td>Pint-Pchannel (b)</td>
<td>0.</td>
<td>0.</td>
<td>2.</td>
<td>2.</td>
<td>2.</td>
</tr>
<tr>
<td>Enrichment (%)</td>
<td>4.5</td>
<td>6.85</td>
<td>4.5</td>
<td>4.5</td>
<td>4.5</td>
</tr>
<tr>
<td>Burn-up (Gwd/t) (max. rod)</td>
<td>63.8</td>
<td>33.</td>
<td>52.8</td>
<td>62.3</td>
<td>64.3</td>
</tr>
<tr>
<td>Corrosion thickness (μ)</td>
<td>80</td>
<td>4</td>
<td>40</td>
<td>80</td>
<td>20</td>
</tr>
<tr>
<td>Gap gas composition</td>
<td>83.3% He + 16.7% Xe</td>
<td>He</td>
<td>He</td>
<td>He</td>
<td>He</td>
</tr>
<tr>
<td>Test energy deposition (cal/g) at 0.4 s</td>
<td>110</td>
<td>211</td>
<td>120</td>
<td>95</td>
<td>105</td>
</tr>
<tr>
<td>(at 1.2 s)</td>
<td></td>
<td></td>
<td></td>
<td>(at 1.2 s)</td>
<td></td>
</tr>
<tr>
<td>Power pulse width (ms)</td>
<td>9.5</td>
<td>9.5</td>
<td>9.5</td>
<td># 60.</td>
<td>9.</td>
</tr>
<tr>
<td>Diametral maximum clad strain (mean value, %)</td>
<td>-</td>
<td>3.5</td>
<td>2.1</td>
<td>not yet available</td>
<td>not yet available</td>
</tr>
<tr>
<td>Rod failure</td>
<td>yes</td>
<td>no</td>
<td>no</td>
<td>no</td>
<td>no</td>
</tr>
<tr>
<td>Maximal clad elongation (mm)</td>
<td>-</td>
<td>10</td>
<td>6</td>
<td>not yet available</td>
<td>not yet available</td>
</tr>
<tr>
<td>Maximal fuel elongation (mm)</td>
<td>-</td>
<td>4</td>
<td>not available</td>
<td>not yet available</td>
<td>not yet available</td>
</tr>
<tr>
<td>Transient FGR (%)</td>
<td>-</td>
<td>5.5</td>
<td>13.4</td>
<td>not yet available</td>
<td>not yet available</td>
</tr>
</tbody>
</table>

Special features of the rods may be noticed:

- the presence of the rim zone in REP Na1, 4, 5 (200 μm thickness) and REP Na3 (100 μm thickness) due to the high burn-up level of the rods,

- the important clad corrosion in REP Na1 (standard cladding) with hydrogen content up to 700 ppm, spallation and scaling off of the zirconia layer (fig. 1).

2) Main features of the quantitative analysis with the SCANAIR code

The SCANAIR code [3] describes the fuel and clad thermo-mechanical behaviour during fast power transients starting from the initial state of the fuel rods given by the irradiation codes such as TOSURA-REP (IPSN) or METEOR (CEA/DRN).

The fuel swelling induced by fission gases is taken into account based on intra-granular and inter-granular gas behaviour.
The deformation induced by the presence of cracks is described and the plastic strains are
given by the Prandtl-Reuss laws.

The fuel creep mechanism is only simulated by a softening temperature but the clad creep is
not described (no clad ballooning description in the present version).

The main points to be compared to the experimental results are the clad and fuel elongations,
the clad plastic deformation if any, and the fission gas release from fuel due to the transient.

III. THE REP Na1 TEST

The main result of this test is the occurrence of an early rod failure when only 11.7 cal/g [5]
has been injected at peak power node (PPN). At the location of the first failure, 8.75 cm above
bottom of fissile length, the energy deposition is 8.9 cal/g due to the axial power profile.
Taking into account the initial fuel enthalpy of 17.1 cal/g the rod failure occurred with a
maximum fuel enthalpy of 29 cal/g at PPN (see fig. 2) and 26 cal/g at failure level.

The rod failure led to fuel ejection into liquid sodium with sodium flow ejection and pressure
peaks of 9-10 b.

The evolution of the channel voiding zone and of the temperature given by the thermocouples
are understood as due to the passage of hot gases from the fuel.

A partial blockage of the test channel has been observed (reduced residual flow).

1) Analysis of the failure scenario

At the time of failure no significant fuel and clad heat-up nor clad deformation are expected as
confirmed by the SCANAIR code which indicates a maximum fuel temperature in the
periphery of the pellet of 600°C at PPN (fig. 3) and no clad plastic deformation.

The detailed examinations of the clad showed multiple failure sites with cracks of brittle type
and axial propagation [6]. They also evidenced the presence of a multitude of hydride
accumulations with crack initiation in the external part.

Such features are similar to the pre-test observations made on twin samples and indicate that
those hydrides were present in the clad before the CABRI test as a result of the scaling-off of
the zirconia layer.

Indeed, in case of high burn-up fuel rods similar to REP Na1, the disappearance of the
zirconia layer at some places has also been observed in association with hydride
accumulation, so called "sun burst" : this can be explained by precipitation of the hydrides due
to creation of a cold point in the clad after contact with coolant (no more thermal insulation by
the ZrO₂ layer).

On the other hand, measurement of H₂ content in the REP Na1 clad after test, indicated no
modification of the mean hydrogen concentration which is a confirmation of the absence of
hydriding during the test.
Concerning the fuel, the main outcomes from the post test examinations are the following:

- large fuel swelling (5 to 10%) occurred along the test pin, after clad failure,
- loss of cohesion at the grain boundary is observed and may result from over pressurisation of the grain boundary gases during the fast power pulse,
- the total amount of fuel loss is evaluated to 6 g (hodoscope) but is not only due to the rim particles entrainment; fuel fragments (\( \varnothing > 40 \mu m \)) from the inner fuel part have also been ejected out of the rod,
- total scaling off of the zirconia layer,
- no release of intragranular gases has been observed (EPMA) indicating that the driving force for fuel pressurisation resulted from inter-granular and porosity gases.

So, as a summary, the rod failure in REP Na1 can be understood as the result of:

- the action of the rim zone in which the rapid temperature transient above the irradiation conditions creates gas pressurisation and fuel fragmentation, loading the clad; indeed at such enthalpy level (\( \approx 30 \text{ cal/g} \)), the classical fuel swelling resulting from diffusion process cannot be activated and cannot take part in the clad loading,
- the low mechanical resistance of the clad due to high corrosion level with spalling zirconia which favours hydride concentration and thus embrittlement.

Such a mechanism is confirmed by the absence of failure in REP Na3 and REP Na5 with high burn-up fuel rods (52.8 and 64.3 GWd/t respectively) and thus presence of rim zone but with lower oxidation level (40 \( \mu m \) and 20 \( \mu m \) respectively) and without spallation.

In the recent test REP Na4 with similar burn-up and oxide thickness as in REP Na1, two parameters have been changed simultaneously: the power transient half-width of 60 ms and the state of the zirconia layer without any spallation. The absence of failure in this test seems to be rather related to the uniform oxidation layer.

Another important outcome from REP Na1 test is that after failure the fuel dispersion occurred with solid fragmented fuel.

2) **Description with SCANAIR**

In the SCANAIR code (version 2.2), the simulation of the rim behaviour is made with the following assumptions:

- the grain boundary fragmentation is due to overpressurisation of the gas bubbles compared to equilibrium state,
- after fragmentation and due to high porosity of the rim zone, the fuel behaves as a mixture of gas and particles (hydrostatic behaviour) with direct loading on the clad.

The application to REP Na1 (fig. 4) shows that at the time of experimental failure, the fuel fragmentation in the rim leads to a sharp increase of the contact pressure (up to 270 b) and thus to a high clad strain rate, with no plastic clad deformation.
Uncertainty on the contact pressure may be related to the knowledge of the initial state of the fuel rod (gas quantity in the rim zone, threshold for fragmentation).

However such contact pressure may lead to clad failure when applied on initially brittle clad at temperature of 280°C and especially with high strain rate of more than 1/s.

At the present time, the clad mechanical behaviour in such conditions is not known and this underlines the need to perform mechanical testing in the transverse direction [7] with clad samples from similar rods as the test rod (local hydriding).

**IV. THE REP Na 2 AND REP Na 3 TESTS**

**IV.1. Analysis of the experimental results**

In the REP Na2 test using a BR3 Rod irradiated to 33 GWd/t with very low oxidation level, the fast power pulse injected 211 cal/g at 0.4 s while in REP Na3, using an EDF segmented rod of 52.8 GWd/t burn-up with 40 μm of oxide layer, the energy injection has been 120 cal/g at 0.4 s.

In both tests, the rod did not fail but significant clad plastic deformation has been obtained.

In REP Na2, there is evidence of a pronounced "bamboo effect" with maximum deformation at the edges of the pellets reaching 4.2 % at PPN while the mean value is 3.5 % (fig. 5). Such feature is the classical result of a plastic deformation due to pellet-clad mechanical interaction with parabolic temperature profile inside the fuel (as in operating conditions).

In REP Na3, the clad deformation shows maximum values at the mid-height of the pellets, the mean value being 2.1 % at PPN (fig. 6). This shape may be explained by a clad loading with maximum fuel temperature in the peripheral part as it occurs in the first phase of fast power transients due to the radial power profile with maximum power in periphery (Pperiphery/Pcenter : 2.56 in REP Na3, 1.9 in REP Na2).

Nevertheless, in both tests, the axial profile of the mean deformation follows the axial power profile (cosine shape).

The occurrence of significant clad deformation is consistent with :

- the transient volume change as shown by the sodium flow expulsion [5] which is more pronounced in REP Na2 than in REP Na3 according to the different amplitudes of clad deformation,
- the measurement of the fuel and clad elongations as given in Table 2.

On the other hand, the destructive examinations of the REP Na2 rod showed :

- a total filling of the pellet dishings which traduces a highly plastic fuel behaviour due to high temperature level in the test,
inside the fuel, the formation of a radial zone in the periphery of the pellets, with high porosity and loss of grain cohesion (fig. 7); this results from fuel fragmentation due to grain boundary separation in the region where maximum fuel temperature is reached.

At the present time, the destructive examinations of REP Na3 are not completed but no evidence of filling of the pellet dishings is found.

The fission gas release measurements indicated a release of 5.5 % in REP Na2 (21.7 cm$^3$ NTP) and 13.4 % (45 cm$^3$ NTP) in REP Na3. Taking into account the difference of the rods burn-up and the associated gas retention, these results show that the fission gas release is highly correlated to the irradiation level.

IV.2. SCANAIR results

Both tests have been calculated with the Scanair code (version 2.2) on the basis of the initial state of the rods given by the TOSURA-REP results and taking into account the presence of the clad oxide layer (thermal effect).

IV.2.1. REP Na2

Due to the high energy injection, the calculated maximum fuel enthalpy (radially averaged) is 206 cal/g (at 95 ms) and the maximum fuel temperature reaches only very temporarily 2815°C (at PPN) which is close to melting temperature.

The fig. 8 shows the radial profile of the fuel temperature at different times in the transient. We can note that after 250 ms, the radial profile becomes of parabolic shape and that high temperature level is maintained in the centre of the fuel (above 2000°C up to 3.5 s): this contributes to activate fuel swelling and explains the clad deformation shape ("bamboo").

The calculated maximum plastic clad deformation reaches 6.5 % (fig. 5) which is over-estimated compared to the mean experimental value of 3.5 % (no description of the bi-dimensional effect in SCANAIR). A sensitivity study has shown that this result cannot be explained by the uncertainty on the clad mechanical properties.

However, such over-estimation is consistent with the absence of description in SCANAIR of the filling of dishings volume due to creep mechanism at high temperature level and with the assumption that the fuel porosity is maintained even under significant fuel swelling (not justified at high temperature level).

Substracting those volume contributions from the fuel swelling would lead to a clad deformation of 4.8 % still higher than the experimental value. On the other hand, the clad deformation which would result of the only contribution of the fuel dilatation (without swelling) would reach 2.2 %: this confirms the role of the fuel swelling in the REP Na2 clad loading, with however over-estimation at high temperature by the present SCANAIR modelling.
Such over-prediction is also reflected by the calculated maximum fuel and clad elongations.

Fuel fragmentation due to the rapid temperature increase is found in the pellet periphery consistently with the evidence of the porous zone seen on the radial cuts.

The calculated fission gas release amounts to $17 \text{ cm}^3 \text{ NTP}$ in reasonable agreement with the measurement ($21.7 \text{ cm}^3 \text{ NTP}$).

**IV.2.2. REP Na3**

The best-estimate description of REP Na3 with SCANAIR has been obtained with the upper limit of energy injection within the uncertainty margin ($125 \text{ cal/g at 0.4 s}$) and led to a maximum fuel enthalpy of $131 \text{ cal/g}$.

The figure 9 shows the radial temperature profile at different times with a maximum temperature of about $2410^\circ\text{C}$ reached in the pellet periphery at 90 ms.

The main difference compared to the REP Na 2 test is the fact that the temperature in the fuel centre stays below $1650^\circ\text{C}$ (higher than $2000^\circ\text{C}$ in REP Na2) which reduces the fission gases induced swelling for clad loading.

In REP Na3, the clad deformation occurred in the first phase of the transient and reached 1.8% (fig. 6) slightly below the experimental value of 2.1%. In REP Na2, such kind of deformation has been erased by the continuous loading due to the high temperature level under cosine radial profile.

The calculated maximum clad elongation ($7.7 \text{ mm}$) and the residual fuel elongation ($1.2 \text{ mm}$) are in reasonable agreement with the experimental values (respectively 6 mm and $3 \text{ mm} \pm 2 \text{ mm}$).

The calculated fission gas release due to migration of intra-granular gases to porosities (free volume) amounts to $20 \text{ cm}^3$ to be compared to $45 \text{ cm}^3$ measured. At high burn-up this might be explained by additional gas release from gases retained inside the porosities at end of irradiation and will be taken into account in the future development of the SCANAIR code.

**V. DISCUSSION OF THE RESULTS AND OPEN ISSUES**

The interpretation of the first CABRI REP Na tests has shown that with irradiated PWR rods, significant clad deformation and even rod failure may be obtained under RIA transients.

These results may be related to the fast power transient ($9.5 \text{ ms half width compared to 30 - 80 ms in reactor situation}$).

Indeed a sensitivity study performed with SCANAIR with different pulse half-widths ($9, 20, 60 \text{ ms}$) has shown that with similar total energy injection, a fast power pulse ($hw = 9 \text{ ms}$) leads to higher fuel temperature and clad deformation than a slower transient ($hw = 20,60 \text{ ms}$).
From the analysis of the tests, we can postulate different phases of the clad loading scenario with rod failure potential during a RIA transient as illustrated by the following scheme:

In a very early phase (some ms), rod failure may occur due to gas pressurisation and fuel fragmentation with brittle cladding (rim behaviour, REP Na1).

In a second phase (up to hundred ms), strong pellet-clad mechanical interaction may occur due to thermal fuel dilatation and swelling and is increased with energy deposition and burn-up level (REP Na2, REP Na3, NSRR tests [4]).

In the reactor situation with pressurised water environment, a third and fourth phases may appear due to departure from nucleate boiling (DNB) occurrence.

In this case, due to the lower clad-coolant heat transfer, the fuel temperature will be maintained at high temperature level during a longer time leading, in spite of clad dilatation, to continuous clad loading if sufficient energy is injected.

Indeed, the REP Na2 rod would have certainly encountered DNB in pressurised water conditions due to less efficient cooling with water compared to sodium and this would have most likely led to rod failure. This might also be true for REP Na3 and REP Na5.

Finally, in a fourth phase, clad ballooning might occur after sufficient gas release and clad heating (long term event) and lead to failure under quenching.

The two last situations are not addressed presently neither by the CABRI REP Na tests nor by other available tests.

In fact, a pressurised water loop associated to an in-pile power transient facility does not exist world-wide.
At the present time, the only investigation in realistic thermal hydraulic conditions concern the determination of clad-coolant heat transfer correlation under fast power transients (out of pile experiments underway in CEA).

A project study to implement a pressurized water loop in the CABRI reactor has been initiated [8] based on the need of representativity for fission gas behaviour, DNB occurrence and post-DNB phenomenology.

Without availability of a fully representative test facility, two major points must be studied by analytical testing for a better description in SCANAIR of the thermo-mechanical rod behaviour:

- the fission gases behaviour from initial state during the whole transient with gas pressurisation in the rim zone, fuel fragmentation, fuel swelling, gas release; analytical tests have been proposed in NSRR facility but their feasibility is not reached,

- the clad mechanical properties, in particular for highly corroded clad as in REP Na1 [7].

VI. CONCLUSION.

The first CABRI REP Na tests have shown that highly irradiated fuel (up to 63 Gwd/t) submitted to a RIA transient may experience early rod failure with solid fuel ejection into the coolant. Such behaviour has been understood as the result of gas pressurisation inside the rim zone and clad embrittlement due to the spallation of the oxide layer and related hydride accumulation.

In case of less oxidised clad (REP Na3, REP Na5) and/or lower burn-up (REP Na2), the experiments have demonstrated a satisfying rod behaviour with nevertheless significant clad deformation due to fuel thermal dilatation and fuel swelling, function of the energy injection.

The fuel ejection in REP Na1 and the evidence of fuel fragmentation in the hot fuel zone in the other tests, underlined the fact that in case of rod failure, fragmented solid fuel (down to 0.1 μm) with associated fission gases is already available for fuel-coolant interaction with a potential high energy conversion rate.

It must be stated that such tests in the CABRI sodium loop are not fully representative of the whole sequence of a RIA due to the impossibility to study the long-term phenomena (occurrence of DNB, post-DNB events).

However, the first analysis with the SCANAIR code has succeeded to explain the mechanism for clad loading but additional knowledge is needed for better quantification of the results in the field of gases behaviour (initial state, transient behaviour) and clad mechanical properties. The analytical experiments which are underway should improve the quantification of the results.

Future REP Na tests will concern the effect of wider power transients and the study of the MOX fuel behaviour at different irradiation levels.
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This meeting

Cladding and fuel modifications of a 60 GWd/t irradiated rod during a power transient
performed in the CABRI reactor.
This meeting

Tensile properties of irradiated zircaloy-4 cladding submitted to fast transient loading.
This meeting.

The project to implement into CABRI a pressurised water loop. Motivations and objectives
of the future test program.
This meeting.
<table>
<thead>
<tr>
<th>Name (date)</th>
<th>Fuel Rod</th>
<th>Maximum mean fuel enthalpy (cal/g)</th>
<th>Remarks</th>
</tr>
</thead>
<tbody>
<tr>
<td>REP Na1 (11/93)</td>
<td>EDF, 63 Gwd/t (4.5%) grid levels 5/6 Fabrice rod: 569 mm</td>
<td>115</td>
<td>failure</td>
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<td></td>
<td></td>
<td></td>
<td>fast pulse</td>
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<td>REP Na2 (06/94)</td>
<td>BR3, 33 Gwd/t (6.85%) no rod conditioning (1 m length)</td>
<td>200</td>
<td>no failure</td>
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<td></td>
<td></td>
<td></td>
<td>fast pulse</td>
</tr>
<tr>
<td>REP Na3 (10/94)</td>
<td>EDF, 52 Gwd/t (4.5%) grid levels 5/6 segmented rod (440 mm)</td>
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<td>no failure</td>
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<td></td>
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<td></td>
<td>fast pulse</td>
</tr>
<tr>
<td>REP Na4 (07/95)</td>
<td>EDF, 63 Gwd/t (4.5%) grid levels 5/6 Fabrice rod: 571 mm</td>
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<td>no failure</td>
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<tr>
<td></td>
<td></td>
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<td></td>
<td>ramp</td>
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<tr>
<td>REP Na5 (05/95)</td>
<td>EDF, 63 Gwd/t (4.5%) grid levels 2/3 Fabrice rod: 571 mm</td>
<td>~115</td>
<td>no failure</td>
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<td></td>
<td></td>
<td></td>
<td>fast pulse</td>
</tr>
<tr>
<td>REP Na6 (95)</td>
<td>Mox 3 cycles Fabrice rod</td>
<td>~140</td>
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<tr>
<td>REP Na7 (96)</td>
<td>Mox 4 cycles Fabrice rod</td>
<td>~125</td>
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<td>REP Na8 (96)</td>
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<td>~125</td>
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<tr>
<td>REP Na9 (96)</td>
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<td>~180</td>
<td>ramp to be</td>
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</tr>
</tbody>
</table>

Table 1: RIA TESTS MATRIX IN CABRI SODIUM LOOP
Oxide thickness on the part of the PWR rod \( \text{QO}_2 \) for CABRI Rep Nal test

**FIG. 1**

**FIG. 2**

Lineic power, injected energy and fuel enthalpy at Peak Power Node RepNa1

- \( H_{\text{max}} = 112.9 \text{ cal/g} \)
- \( E_{\text{inj}} = 110 \text{ cal/g at 0.4s} \)
- \( P_{\text{lin max}} = 216 \text{ kW/cm} \)

half-width 9.5 ms
RepNa1: temperature (radial distribution) at failure time (# 74 ms)
SCANAIR results

![Graph showing temperature distribution](image)

Z = 260.97 mm (PPN)
Z = 71.17 mm

Power profile

RIM

600°C

FIG. 3

RepNa1: clad elasto-plastic strain and contact pressure at failure location
SCANAIR results

![Graph showing strain and pressure](image)

Clad inner hoop elasto-plastic strain
Contact pressure

FIG. 4
Temperature radial profile

Porosities and dishes filled, fuel swelling maintained

Porosities and dishes filled, fuel swelling suppressed

Calculated clad plastic hoop strain axial profile

Axial profile of clad plastic hoop strain reference case REPNA3

Average value = 2.11%

Measured at 0° 1.77%

Last non oxidised mesh

Elevation (mm/BFC)
Radial cut at 519.5 mm from bottom of fissile column

FIG. 7
REP-Na2 SCANAIR 2.2 recalculations

Fuel temperatures radial profiles at PPN location at different characteristic times

**FIG. 8**

RepNa3: radial temperature profiles SCANAIR results at PPN

**FIG. 9**
Development and Performance of a Research Program for the Analysis of High Burn-up Fuel Rod Behavior under RIA Condition in the IGR Pulse Reactor

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Abstract

The paper briefly characterizes the research program to study the fuel rod behavior of Russian PWRs with high burn-up fuel under RIA conditions, presents the main results of reactor tests of 13 high burn-up fuel rods, of 10 fuel rods with pre-irradiated cladding, of 20 fresh fuel rods; and analyzes in a preliminary way the observed results from the point of view of determining threshold disruptive energy deposition values and fuel element failure mechanisms.

1. Introduction

During the period from 1983 to 1992, specialists of the Russian Research Center "Kurchatov Institute" (RRC KI) prepared and implemented a research program of experimental studies of fuel rod behavior of Russian PWR (WWER-1000 type) under RIA conditions. This program, implemented in cooperation with specialists of a number of institutes of the ex-USSR, covered about 150 reactor capsule experiments aimed at studying thermo-mechanical aspects of LWR type fuel rod behavior under RIA conditions. The effects of the following parameters on failure mechanisms and threshold disruptive characteristics were studied during the program implementation: power pulse half width, fuel-cladding gap, initial pressure drop at the fuel cladding, coolant type, energy deposition in the fuel. The final stage of the program was a series of experiments with the main purpose to study burn-up effects on the threshold characteristics of fuel rod failure. This series of experiments was prepared and conducted by RRC KI specialists together with a number of research institutes of the ex-USSR:

- Research Institute of Atomic Reactors, Dimitrovgrad - design and manufacturing of re-fabricated fuel rods and capsule devices, conduct of pre- and post-reactor studies of fuel rods;
- Joint stock company MS, Electrostal - design and manufacturing of non-irradiated fuel rods;
- Joint Expedition, Research Institute "Luch" Scientific and Industrial Association, Semipalatinsk - preparation and conduct of reactor tests of the fuel rods.

MINATOMENERGOPROM of the USSR was the sponsor of the program as a whole.

The preparation and conduct of this series of experiments was carried out during the period of 1990-1992 using commercial reactor fuel of WWER-1000 type, irradiated to an average burn-up of about 48 MWd/kgU. The following were the main tasks of the series:

- to determine energy thresholds of fuel rod damage;
- to study comparative features of mechanisms of fuel rod deformation and failure with fresh and irradiated fuel;
• to prepare recommendations on the possibility of using the fresh fuel data base to validate nuclear power plant safety during many power cycles of operation;
• to validate the sufficiency of the available data base on fuel rod behaviour of LWR type under RIA conditions and to prepare recommendations for further research.

Unfortunately, due to the economic situation in Russia at the beginning of 1990s, as well as general decrease in the interest in RIA studies at the end of the 1980s, the research work under this program was frozen at the stage of completing the post-test studies. The revival of the research under the program was caused by an urgent need to validate the Russian and the world safety standards and by unexpected results of experiments with high burn-up fuel conducted in the CABRI reactor (CEA, Cadarache) within the framework of a similar IPSN program in 1993-1994 [1]. The result was the restart of the work to update the data base on the behavior of Russian fuel elements of PWR type with high burn-up fuel under RIA conditions, which started in 1995 under agreements between NSI RRC KI - US NRC (USA) and NSI RRC KI - IPSN (France), and with financial support of the Russian Ministry of Science.

The research is carried out in the RRC KI together with specialists of the following institutes:
• Research Institute of Atomic Reactors, Dimitrovgrad, Russia;
• Institute of Atomic Energy of National Nuclear Center of Kazakstan Republic, Semipalatinsk, Kazakstan.

The analysis of the results presented in this paper is of a preliminary nature.

2. Experimental procedures.

Three types of fuel rods were used as test specimens:
• fuel rods re-fabricated from commercial fuel elements of the WWER-1000 type, subjected to many power cycles of operation at power unit No. 5 of Novovoronezh Nuclear Power Plant (NV NPP), (Type - C);
• fuel rods, manufactured using cladding of commercial fuel elements of power unit No. 5 of NV NPP and fresh fuel of the WWER-1000 type (Type - D);
• fuel rods manufactured from non-irradiated materials in accordance with the main provisions of the WWER-1000 technology (Type - E).

Fig. 1 shows design schemes of all three types of fuel rods. The manufacturing technology of C-type fuel rods included cutting the irradiated commercial fuel rod into samples of required length, removal of fuel from the top and bottom parts of each sample, installation of the rod hardware (low and upper caps, coil spring, connector, etc) and its sealing. To manufacture D-type fuel rods, the fuel was completely removed from the commercial fuel rod and the cladding was filled with the fresh fuel. The manufacturing of E-type fuel rods was carried out basically according to the standard manufacturing technology for WWER-1000 fuel elements.
Table 1 contains source characteristics for each of the three types of fuel rods.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>C</th>
<th>D</th>
<th>E</th>
</tr>
</thead>
<tbody>
<tr>
<td>1. Fuel</td>
<td></td>
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<tr>
<td>1.1 Initial composition before power operation</td>
<td>UO₂</td>
<td>UO₂</td>
<td>UO₂</td>
</tr>
<tr>
<td>1.2 Initial enrichment, %</td>
<td>3.58</td>
<td>4.46 - 4.47</td>
<td>4.46 - 4.47</td>
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<tr>
<td>1.3 Burn-up, MWd/kgU</td>
<td>41 - 49.7</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>1.4 Outer pellet diameter, mm</td>
<td>7.55 - 7.6</td>
<td>7.56</td>
<td>7.56 - 7.69</td>
</tr>
<tr>
<td>1.5 Density, g/cm³</td>
<td>10.31 - 10.32</td>
<td>10.5 - 10.6</td>
<td>10.5 - 10.6</td>
</tr>
<tr>
<td>1.6 Fuel stack length, mm</td>
<td>148 - 167</td>
<td>141 - 143</td>
<td>142 - 144</td>
</tr>
<tr>
<td>2 Cladding</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>2.1 Initial composition before power operation</td>
<td>Zr-1%Nb</td>
<td>Zr-1%Nb</td>
<td>Zr-1%Nb</td>
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<tr>
<td>2.2 Outer cladding diameter, mm</td>
<td>9.13</td>
<td>9.11</td>
<td>9.15</td>
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<tr>
<td>2.3 Inner cladding diameter, mm</td>
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<td>not measured</td>
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<td>2.4 Oxide thickness, μm</td>
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<td>5-6</td>
<td>3-5</td>
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<tr>
<td>3 Fuel rod</td>
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<td>3.1 Fuel rod length, mm</td>
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<td>3.2 Initial fill gas pressure, MPa</td>
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<tr>
<td>3.3 Gas composition</td>
<td>He</td>
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<td>He</td>
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<tr>
<td>3.4 Free gas volume, cm³</td>
<td>6.11</td>
<td>5.80</td>
<td>6.59</td>
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<tr>
<td>3.5 Fuel-cladding gap, mm</td>
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<tr>
<td>3.6 Number of fuel rods</td>
<td>13</td>
<td>10</td>
<td>20</td>
</tr>
</tbody>
</table>

The tests of the fuel rods under RIA conditions were conducted in the pulse research reactor IGR (IAE, Semipalatinsk, Kazakhstan). The IGR reactor (Fig. 2) is a homogeneous graphite reactor where the power pulse is produced by withdrawal of control rods from the core. The back edge of the power pulse may be formed both as a result of the negative temperature reactivity coefficient and by insertion of control rods into the reactor core.
The test fuel rods were positioned in the experimental capsule which, in turn, was installed in the central cavity of the IGR reactor (Fig.3). Each of 23 experimental capsules contained, as a rule, two fuel rods: one re-fabricated rod and one fresh rod. Either water or air was used as the coolant in the capsule at standard initial conditions (20°C, 0.1 MPa). RIA modes were simulated by producing power pulses in the IGR reactor (Fig.4). There were no measurements of thermo-physical parameters of the fuel rods and the capsule during the tests.

The methodological principles of the test program were developed taking into account the following main constraints:

- absence of reliable a priori information on failure mechanisms and thresholds of fuel elements with high burn-up;
- absence of measurement systems which allow the diagnostics of fuel rod integrity during the test process;
- absence of neutron radiographic facility at the IGR reactor, and impossibility of examination of fuel rod conditions immediately after the tests;
- impossibility, because of safety requirements, of opening capsules and examining fuel rods after the test directly at IGR reactor site.

The consideration of these constraints resulted in the development of a test program consisting of two stages. During the first stage (13 experiments), the energy deposition was increased step-by-step sequentially starting from the lowest level (within conservative estimates). After the completion of the first stage of the program, thirteen capsules with fuel rods were delivered to hot cells of RIAR (Dimitrovgrad, Russia), where they were opened and examined. Based on examination results, adjustments were introduced in the test modes of the second stage of the program (10 experiments). The presence of the above constraints has also resulted in the necessity of carrying out a rather complex
procedure for a reliable determination of the ratio between the reactor energy deposition and the energy deposition in fuel of re-fabricated fuel rods.

Fig. 4 IGR Reactor Power Shapes during High Burn-up Fuel Rod Tests

3. Experimental results and their analysis.

Fig. 5 presents a summary of test results for the following groups of fuel rods:
- 8 fuel rods of C type (coolant - water);
- 5 fuel rods of C type (coolant - air);
- 5 fuel rods of D type (coolant - water);
- 5 fuel rods of D type (coolant - air).

Fig. 5 Summarized Results for High Burn-up Fuel Rod Tests under RIA Conditions
The following can be noted at the stage of preliminary analysis of data in Fig.5:

- for fuel rods of C and D type, tested in water, the range of fuel cladding failure threshold was identified: 195 - 261 cal/g-fuel for type C and 199-323 cal/g-fuel for type D;

- for fuel rods of C and D type, tested in the air, the threshold of the fuel cladding failure was not identified, but the energy deposition range for the cladding fragmentation threshold was determined: 188-285 cal/g-fuel for type C and 233-326 cal/g-fuel for type D.

The next stage of result analysis includes consideration of additional experimental results required for a more strict determination of the cause-consequence relations that influence fuel rod damage. An important circumstance, that defines the approach to the analysis of threshold disruptive loads, is the information on fuel rod failure mechanisms which can be obtained as a result of a combined study of

![Fig. 6 Appearance of Fuel Rods Deformation](image)

the dynamics of the fuel and cladding behavior for various energy deposition levels. Fig. 6 shows photos of the appearance of C and D type fuel rods for the lowest energy deposition values at which cladding failures were detected. Results in Fig. 6 demonstrate that in all cases the mechanism of cladding failure was its plastic deformation due to inner pressure. This failure mechanism is similar to the failure mechanism of a fresh fuel rod. It should be noted that many previous tests of fresh fuel elements of the WWER-1000 type demonstrated that failure of fuel cladding loaded with excess inner pressure takes place near of the phase transition (α - β) in the zirconium and corresponds to the temperature of about 900 °C. A similar mechanism (plastic deformation) was also detected for fresh fuel rods loaded with an external pressure of ~14 MPa (the cladding failure in this type of fuel rods takes place at the same temperature). For fresh fuel rods with no initial pressure drop at the cladding, there is no loss of cladding integrity until the time of its melting.

Thus, we can make a reliable conclusion that in this series of tests there were no cases of low temperature brittle failure of fuel rods both at the stage of their heating and cooling. The analysis of data in Fig.5 for the conditions of C type fuel rods, cooled with water, after the test, allows the conclusion
Fig. 5 for the conditions of C type fuel rods, cooled with water, after the test, allows the conclusion that
the failure threshold of this group of fuel rods is in the energy deposition range of 195 - 261 cal/g-fuel.
Some inconsistencies in the results within this group of fuel rods can probably be explained by errors in
determining energy deposition values in fuel rods. The value of 240 cal/g-fuel, average for the fuel rod
group, can be adopted as the threshold disruptive energy deposition for this group of fuel elements at
this stage.

The result analysis for D and E type fuel rods, cooled with water, shows no major differences in
threshold disruptive energy deposition values:

- type D - 320 cal/g-fuel (initial cladding deformation, no cladding oxidation);
- Type E - 310 cal/g-fuel (initial cladding deformation, no cladding oxidation).

For fuel rods of these types the value of 320 cal/g-fuel can be considered as the average threshold dis-
ruptive energy deposition.

An important matter for comparison of the observed results with results of other studies is the determi-
nation of the maximum enthalpy of the fuel, which corresponds to the threshold energy deposition. This
is of particular importance since the IGR reactor experiments were conducted with power pulses, that
describe rather realistically RIA conditions in commercial reactors, which half width is considerably
different from conditions of power pulses for a majority of similar experiments conducted at reactors
PBF (USA), NSRR (Japan), CABRI (France) /1,2,3/. Correct calculation of the peak enthalpy of the
fuel is a rather complex problem and has not yet been done. However some preliminary assessments
were made; their results are presented below.

Parameters that characterize the conditions of fuel pellets of C, D E type fuel rods after the tests were
used as the source data for the assessment (Table 2).

Comparative analysis of fuel pellet conditions after the tests, carried out on the basis of data in Table 2
and data base of previous tests of E type fuel rods, shows that above a certain level of the energy
deposition in the center of the fuel pellet, the area can be positively identified, where the temperature
exceeded the fuel melting point during the test. Thus, according to Table 2, there is a boundary of
melting in the central part of fuel pellets in the energy deposition range of 400-410 cal/g-fuel. The
analysis of the photos of fuel pellet cross sections in the table shows that the range of 320-360 cal/g-
fuel is the lower threshold range of energy depositions for these processes. In two out of four fuel rods
of this range, conditions were detected that directly preceded molten fuel flow down to the bottom part
of the fuel rod. These conditions are characterized by local areas of fuel micro-melting in the volume,
thus leading at the initial phase of the process to disappearance of the central hole in the fuel pellet and
to formation of a characteristic macro-pore structure of the fuel in the center of the pellet. Using this
diagnostic factor, it can be assumed with sufficient accuracy that energy depositions produced in these
fuel rods correspond to the peak enthalpy of the fuel at its melting point. Fig. 7 shows the time depend-
ence of energy deposition in the fuel center for each fuel rod of the group of 320-360 cal/g-fuel range.
The analysis of the results in Fig.7 demonstrates that the temperature in the fuel center reaches the
melting point in those cases when the total energy deposition over the whole process duration is some-
what higher than the fuel melting enthalpy. This is confirmed by data of temperature measurements in
the fuel center and at fuel rod cladding surface obtained in previous experiments with fresh fuel, which
are presented in Fig. 8. The comparison of time dependencies for the
Table 2. Appearance of Fuel Rod Cross-Section after IGR Tests

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reactor power and fuel temperature in fuel rods shows that the higher is the absolute value of the maximum fuel temperature in the tests, the larger share of the energy deposition is spent to increase the fuel enthalpy; this is related to the heat exchange mode at the fuel rod surface, which is characterized by fuel cladding temperature changes. The data presented on the fuel temperature confirm that practically all energy deposition is used to get the fuel temperature of about 2500 °C and above, in the time interval of 0-5 sec. Thus, the peak enthalpy in the fuel center can be determined using $\gamma$ coefficient, which is obtained using data in Fig. 7 as the ratio between the fuel melting enthalpy and energy deposition for fuel rods tested at the boundary of fuel melting.

The average value of $\gamma$ coefficient determined using the described procedure was 0.9 for fuel rods with the fresh fuel and 0.84 for fuel rods with the high burn-up fuel.
In this case the peak enthalpy in the fuel center, which corresponds to the fuel rod cladding failure threshold, is 202 cal/g-fuel for fuel rods of C type, cooled with water. If this enthalpy is obtained, the maximum temperature of the fuel cladding reaches 900°C and the result is cladding failure of ballooning type.

Since as seen in Fig. 5, the failure threshold of fuel rods in the case of air cooling was not observed, the following approach was used to determine its value:

\[ E = \gamma I \left( \underbrace{\text{under } T_{\text{fuel}}} \right) \]
\[ T_{\text{fuel}} = T_{\text{cl}} + \Delta T, \]

where

- \( E \) is the energy deposition in the fuel rod;
- \( \gamma \) is the coefficient of relations between the energy deposition and the peak enthalpy in the fuel;
- \( I \) is the peak enthalpy in the center of the fuel;
- \( T_{\text{fuel}} \) is the temperature in the fuel center;
- \( T_{\text{cl}} \) is the fuel cladding temperature;
- \( \Delta T \) is the temperature difference between \( T_{\text{fuel}} \) and \( T_{\text{cl}} \).

In accordance with many experimental data obtained previously for E type fuel rods, \( \Delta T \) for the rod failure threshold (\( T_{\text{cl}} = 900°C \)) is 150°.

The values of the threshold disruptive energy deposition for fuel rods tested in the air, obtained using the above procedure, is 90 cal/g-fuel for D, E types and 84 cal/g-fuel for C type.

The assessment of fuel rod fragmentation threshold can be made on the basis of the following data:

1) **Coolant - water**:

- energy deposition of 409 cal/g-fuel (fuel rod of C type) - no fragmentation;
- energy deposition of 424 cal/g-fuel (fuel rod of E type) - fragmentation of the fuel rod;

The fragmentation threshold is 420 cal/g-fuel (determined as the average value).

2) **Coolant - air**:

- energy deposition of 284 cal/g-fuel (fuel rod of C type) - destroyed into several pieces which preserved the radial shape of the fuel rod;
- energy deposition of 326 cal/g-fuel (fuel rod of D type) - destroyed into small fragments.

The fragmentation threshold is 280 cal/g-fuel.

**Conclusions**

The preliminary analysis of the data base resulting from the reactor tests under RIA conditions of 23 pre-irradiated fuel rods and of 20 non irradiated fuel rods for Russian PWRs has demonstrated that for fuel rods, loaded with inner gas pressure, the threshold disruptive mechanism is plastic deformation of the cladding of the ballooning type with subsequent cladding rupture.
The threshold values of energy deposition resulting in fuel cladding rupture are as follows:

- 240 cal/g-fuel for high burn-up fuel rods, cooled with water;
- 84 cal/g-fuel for high burn-up fuel rods, cooled with air;
- 320 cal/g-fuel for fresh fuel rods, cooled with water;
- 90 cal/g-fuel for fresh fuel rods, cooled with air.

Fuel rod fragmentation thresholds are as follows:

- 420 cal/g-fuel for fuel rods, cooled with water;
- 280 cal/g-fuel for fuel rods, cooled with gas (air).

There were no significant qualitative differences detected in the nature of failure of three types of tested fuel rods:

- fuel rods with burn-up of about 48 MWd/kgU;
- fuel rods with pre-irradiated cladding and fresh fuel;
- non-irradiated fuel rods.

The peak enthalpy in the fuel center relative to the energy deposition value is 0.9 for fuel rods with the fresh fuel and 0.84 for fuel rods with the high burn-up fuel.

References


Primary Factors Causing the Failure of High-Burnup LWR Fuel Rods During Simulated Reactivity Initiated Accidents

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Abstract

This paper briefly describes the behavior of fuel rod cladding as a function of burnup: reviews the experimental data that have been obtained from reactivity initiated accident (RIA) tests in the SPERT-CDC, PBF, NSRR, CABRI and IGR test reactors to date; discusses the two separate physical phenomena, (1) cladding and fuel heatup, and (2) pellet-cladding-mechanical-interaction (PCMI), which lead to fuel cladding failure; points out the importance of waterside corrosion and cladding hydriding on cladding failure of high burnup fuel, and indicates that, if the test rod cladding has little corrosion, and consequently little hydriding, the fuel failure mechanism will change from hydride assisted PCMI to a cladding and fuel heatup failure mechanism.

Introduction

Review of the experimental results from integral tests and analyses of these results, both of which are described in the previous papers presented during this Session(1-6), together with limited FRAP T-6(7) computer code calculations and a basic understanding of the behavior of zircaloy cladding, were used to determine the primary factors causing the failure of high-burnup fuel rods during simulated reactivity initiated accidents (RIAs). Two general phenomena, cladding and fuel heatup, and the expansion of the fuel pellet against the cladding, are responsible for the failure of both irradiated and unirradiated light water reactor (LWR) fuel rods during simulated RIAs. The specific fuel rod failure mechanisms that result from cladding and fuel heatup during an RIA are: (a) ballooning and burst of ductile zircaloy resulting from cladding heatup and the increase in internal fuel rod pressure; (b) cladding embrittlement that occurs during the phase transformation from alpha to beta phase zircaloy; (c) thickening and thinning of the zircaloy cladding when it has reached temperatures high enough for plastic flow(8); and (d) melting. The specific fuel rod
failure mechanism that results from fuel pellet expansion is pellet-cladding mechanical interaction (PCMI), which can be enhanced by hydrogen uptake that results from waterside corrosion. Waterside corrosion increases with increasing burnup resulting in hydrogen uptake increasing with increasing burnup thereby decreasing the radially averaged axial peak fuel enthalpy required to cause cladding failure with increasing burnup. The objectives of this paper are to review the behavior of the cladding, discuss the important experimental results and demonstrate the various cladding failure mechanisms, and present conclusions.

Cladding Behavior

The behavior of zircaloy cladding is strongly influenced by burnup. The cladding becomes less ductile and the strength increases with increasing fluence. Zircaloy cladding also becomes less ductile with increasing hydrogen content and the tensile strength decreases with increasing hydrogen content. The detrimental effects of hydrogen in zircaloy cladding have long been realized. In the early days of the fabrication of zircaloy clad UO2 fuel rods for LWRs, the moisture content of the UO2 was not always controlled closely enough. Moisture uptake by the fuel pellets during fabrication could lead to cladding failure during nuclear operation as a result of the following zircaloy hydriding process. During nuclear operation the moisture in the fuel pellets was radiolytically decomposed into atomic hydrogen and OH radicals. The free hydrogen traveled down the temperature gradient to the fuel-cladding gap. Because the cladding inside surface was oxidized, the hydrogen did not enter the cladding until a scratch or pit in the internal oxide layer occurred (i.e., by a scratch caused by pellet cladding contact, etc.). The hydrogen then entered the the cladding and formed "sunbursts" or "blisters" on the inside surface of the cladding that grew until they reached the outside cladding surface, whereupon the cladding failed. Examples of a small sunburst, and a large sunburst are shown in Figures 1(10) and 2(11), respectively. This problem was solved long ago by controlling the UO2 pellet moisture content during fabrication. However, the hydriding that has occurred in the cladding of high-burnup RIA test fuel rods discussed in this paper occurred as a result of the corrosion of the outside surface of the cladding as described below.

Corrosion (oxidation) of the outside surface of LWR fuel rods increases with increasing burnup as illustrated in Figure 3(12,13). As shown in Figure 3 the oxide thickness increases with burnup in a linear fashion up to a burnup of about 38 GWD/MTU at which point the oxidation rate
increases dramatically for "high tin" zircaloy and also increases for "low tin" zircaloy. The corrosion of the outside surface of zircaloy cladding results in a 10 to 20% uptake of hydrogen in the zircaloy cladding as illustrated in Figure 4(14), which also illustrates the total hydrogen uptake in zircaloy as a function of corrosion thickness. As can be observed in Figure 4, at an oxide thickness of about 100 microns the hydrogen uptake in the cladding is 600 to 700 ppm. Figure 5(14) shows the brittle fracture of an LWR fuel rod after exposure to an out-of-pile burst test. The test rod, which failed by brittle fracture with long longitudinal splits at two azimuthal locations, had a local burnup of 61.7 GWD/MTU and a hydrogen content of 300 ppm. The longitudinal cracks are very narrow in width as expected for brittle fracture.

When the hydrogen solubility limit is reached in zircaloy, zirconium hydrides precipitate in the circumferential direction in the cladding. At an oxide thickness of about 100 microns, ZrO2 spalling begins. ZrO2 spalling increases the heat transfer and cools the cladding in the region where the spalling occurs. With this cladding temperature decrease, hydrogen builds up and makes an oxide spalled region a probable site for cladding brittle fracture caused by PCMI. Thus hydride assisted PCMI failures are expected(a).

At about 623 K the zirconium hydride platelets begin to go back into solution(14) and at 960K both the zirconium hydride induced embrittlement and the radiation induced embrittlement are annealed out(15). Above 960 K zircaloy is ductile again and therefore ductile behavior is expected above this temperature. If the cladding has not already failed and the fuel rod pressure exceeds the external pressure the fuel rod will balloon and may burst. The amount of ballooning will depend upon the crystalline phase of the zircaloy. Zircaloy is in the close packed hexagonal (alpha) phase up to 1093 K, in the alpha plus beta (body centered cubic) phase between 1093 K and 1248K and in the beta phase above 1248 K(15). Large amounts of ballooning are expected in alpha phase zircaloy, much less ballooning is expected in alpha plus beta phase zircaloy, and then ballooning again increases in beta phase zircaloy(16).

(a) To the Authors' knowledge, the first use of the term "hydride assisted PCMI" was by A. M. Garde, Author of Reference 14.
RIA Experimental Results

The results of RIA experiments performed in the NSRR reactor(1,2), the CABRI reactor(3,4,5), the IGR reactor(6), and the SPERT-CDC and PBF reactors(8) are shown plotted in Figure 6 with identifying numbers for each data point. The test corresponding to each data point number in Figure 6 is identified in Table I. The RIA data are plotted as radially averaged, axial peak fuel enthalpy versus burnup. As can be observed in Figure 6 the data demonstrate that the radially averaged peak fuel enthalpy required to cause fuel rod failure during an RIA decreases with increasing burnup. For the CABRI, NSRR, SPERT-CDC, and PBF experiments very narrow power burst widths (burst width at half maximum power of 9.5 to 24 milliseconds) were used whereas for the IGR tests a burst width of approximately 700 milliseconds was used.

The first evidence of a PCMI induced failure during a simulated RIA occurred during a SPERT-CDC test (CDC-859) where the cladding failed by three long longitudinal cracks typical of brittle fracture(8). SPERT-CDC tests were performed at room temperature in non-flowing water, at atmospheric temperature. The test rod, which was preirradiated to a burnup of 31 GWD/MTU, failed at a radially averaged peak fuel enthalpy of 85 cal/g. However, in addition to the three large cracks in the rod a small longitudinal crack occurred in the region of a blister as shown in Figure 7 which indicates how the cracks were initiated. Oxidation layer thickness was not measured on this rod. A similar fuel rod was tested in the SPERT-CDC (CDC-756), which had a burnup of 32.7 GWD/MTU. This test rod failed by one small longitudinal crack at a radially averaged peak fuel enthalpy of 143 cal/g. PCMI type failures also occurred during a PBF RIA test (PBF-RIA-1-2) using a low burnup (4 GWD/MTU) test fuel rod. The test on the irradiated fuel rod was conducted at hot startup reactor conditions and the cladding failed by 22 PCMI-induced, small longitudinal cracks. The lowest energy deposition at which a PCMI crack occurred was 147 cal/g(8) and posttest metallographic examinations revealed that the cracks occurred very early in the transient possibly near or before the time of peak fuel enthalpy. These were brittle fractures and the reason for the occurrence of brittle PCMI fractures at such a low burnup was never satisfactorily explained even though a detailed posttest examination was performed.

All of the test fuel rod failures that have occurred thus far in the NSRR high burnup experimental program have had zirconium hydride platelets in the zircaloy cladding and have failed by a PCMI mechanism. In fact, the
hydrogen content has been high enough in the cladding to cause an extensive amount of ZrH platelets to precipitate. The ZrH platelets formed in the circumferential direction in the cladding. In two separate NSRR tests "blisters" formed on the outside of the cladding that contained an extremely large amount of ZrH platelets and failures occurred at these blisters. These blisters are very similar to blisters or "sunbursts" that formed on the inside surface of zircaloy cladding in the early years of UO₂ fuel rod manufacture. The two NSRR test fuel rods with blisters (JM-4 and JM-5) had burnups of 21.2 and 25.7 GWd/MTU and failed at radially averaged peak fuel enthalpies of 168 and 158 cal/g of UO₂, respectively. The lowest energy deposition at which an NSRR test fuel rod has failed is 60 cal/g during test HBO-1 which had a burnup of 50 GWD/MTU. This test fuel rod failed by long longitudinal cracks similar to the CDC-859 rod. A similar test rod (HBO-3) with a much thinner oxide layer after preirradiation to 50 GWD/MTU was exposed to 75 cal/g radial averaged peak fuel enthalpy and did not fail.

Evidence of hydriding that causes cladding failure at reduced radial average peak fuel enthalpies has also been suggested in other tests. The fuel rod cladding in the CABRI test, REP-Na₁, where the high-burnup (63 GWD/MTU) fuel rod failed at 30 cal/g of UO₂ radially averaged peak fuel enthalpy, contained a large amount of hydrogen (760 ppm). Posttest examination of the REP-Na₁ test rod revealed oxide spalling and ZrH concentration. Test rod REP-Na₅ had a much thinner oxide layer than test rod REP-Na₁ and consequently a much smaller hydrogen content in the cladding. Test rod REP-Na₅ was exposed to a power burst similar to test rod REP-Na₁, reached a radially averaged peak fuel enthalpy of 100 cal/g and did not fail.

Thus it is clear that the fuel rod cladding failures for all of the test fuel rods in the SPERT-CDC, NSRR and CABRI RIA tests for burnups above about 20 GWD/MTU were caused by hydride assisted PCMI. Since the oxidation and consequently the hydriding increase with increasing burnup, the radially averaged peak fuel enthalpy at failure for test rods that fail by hydride assisted PCMI would be expected to decrease with increasing burnup, especially above 40 GWD/MTU and more so for "high tin" zircaloy than for "low tin" zircaloy.

The radially averaged peak fuel enthalpy for IGR test results for test rods H1T through H8T plotted in Figure 6 were calculated from total energy depositions and power burst shapes(6) and the FRAPT-6 computer code assuming no oxidation of the zirconium 1% niobium cladding. Little
oxidation is expected for the Zr 1%Nb cladding so failure did not occur by hydride assisted PCMI at low temperatures. Cladding temperatures calculated for the four fuel rods that failed in a water environment (H5T, H7T, H2T and H3T with FRAP T-6 calculated radial averaged peak fuel enthalpies of 134, 158, 227, and 262 ca/g, respectively) are higher than the cladding embrittlement and ZrH annealing temperature of 960 K. The FRAP T-6 calculated enthalpies plotted in Figure 6 are not expected to be in precise agreement with the enthalpies that will have been reported by the Kurchatov Institute because the MATPRO (15) code (used for materials properties in the FRAP T-6 code) does not contain properties for the Zr 1%Nb cladding and the FRAP T-6 model was not an exact model of irradiated IGR test rods. It is evident from Reference 6 that test rods H5T, H7T, and H2T failed by ballooning and burst even though the cladding of rod H2T subsequently approached or reached cladding melting. The cladding of test rod H3T melted but probably ballooned and burst before reaching melting.

Thus, an important question is "which failure mechanism will dominate for postulated RIAs in commercial LWRs?". The experimental evidence has shown that the failure mechanism for high burnup test rods changes from hydride assisted PCMI cladding failures (at low enthalpies) to cladding and fuel heatup failure mechanisms (at much higher enthalpies) depending upon the amount of oxidation and hydriding that occurs during the burnup period. The oxidation that occurs for "low tin" zircaloy and for zirconium-niobium alloys is much less than for "high tin" zircaloy and consequently the "low tin" and zirconium-niobium alloy cladding is expected to either not fail; or to fail by cladding and fuel heatup mechanisms at high enthalpies. The high enthalpies required for failure of "low tin" zircaloy and zirconium niobium alloys by cladding and fuel heatup mechanisms may not be achievable for high burnup fuel rods in commercial LWRs. The "high tin" zircaloy with large oxidation, hydriding and oxide spalling is expected to fail at low energy depositions by hydride assisted PCMI. Unfortunately, all of the observed PCMI failures for high-burnup fuel rods have occurred for narrow burst width (9.5 to 24 milliseconds) power pulses, whereas all of the heatup failures for high-burnup fuel rods have occurred for wide burst width (about 700 milliseconds) power pulses, so the effects of power pulse burst width cannot be ruled out. However, all of the observations can be explained by hydrogen embrittlement and therefore power pulse burst width may not be an important factor.
Conclusions

The hydride assisted PCMI failure mechanism has been responsible for all of the RIA test fuel rod failures that have occurred for the SPERT-CDC, NSRR and CABRI experiments for burnups larger than about 20 GWD/MTU. Because hydriding is the result of waterside corrosion (oxidation), and oxidation increases with increasing burnup, especially above 38 GWD/MTU, the radially averaged peak fuel enthalpy required for fuel rod failure is expected to decrease with increasing burnup which is confirmed by the experimental data. Waterside corrosion and consequently hydriding is much less for "low tin" zircaloy and zirconium niobium alloys than for "high tin" zircaloy, and hydride assisted failures of "low tin" zircaloy and zirconium niobium alloys have not occurred in the RIA experiments. The fuel failure mechanism changes from hydride assisted PCMI for high burnup, "high tin" zircaloy to cladding and fuel heatup failure mechanisms that occur at high enthalpies for "low tin" zircaloy and zirconium niobium alloys. The high enthalpies required for failure of "low tin" zircaloy and zirconium niobium alloys by cladding and fuel heatup mechanisms may not be achievable for high burnup fuel rods in commercial LWRs.

References


Table

Table 1. Reactor fuel test designation corresponding to numbers on data plotted in Figure 6.

Figures

Figure 1. Photograph of beginning of hydride "sunburst" on inside of zircaloy cladding.

Figure 2. Photograph of large hydride "sunburst" on inside of zircaloy cladding.

Figure 3. Typical range of PWR ZrO2 thickness versus burnup.

Figure 4. Hydrogen uptake as a function of oxide thickness for zircaloy-4 cladding irradiated in PWRs.

Figure 5. Burst opening region of cladding specimen with 300 ppm hydrogen and a local burnup of 61.7 GWD/MTU.

Figure 6. Radially averaged peak fuel enthalpy for CDC, PBF, NSRR and CABRI Tests plotted versus burnup. Test identification corresponding to numbers on figure are given in Table 1.
Figure 7. Photograph of "blister" surrounding longitudinal split on surface of CDC Test Rod 859.
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Figure 2. Photograph of large hydride "sunburst" on inside of zircaloy cladding.
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Table 1. Test Number on Figure 6 with Test Identification.

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Table 1. Reactor fuel test designation corresponding to numbers on data plotted in Figure 6.
Figure 7. Photograph of "blister" surrounding longitudinal split on surface of CDC Test Rod 859.
Unexpected Transient 
on an Experimental Instrumented Fuel Segment in BR2

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Abstract

During the ultimate checking of an experimental device in the BR2 test reactor under starting conditions, a non-programmed reactivity excursion of the reactor occurred, causing severe damage to an experimental fuel segment loaded in an experimental channel.

The purpose of the experiment was to submit a UO₂ fuel segment, cut from a "mother rod" preirradiated to high burnup (70.3 MWd/kg U) in the BR3 PWR, to a stepwise power increase, in order to determine, in-pile, the fuel thermal conductivity. The fuel segment consisted of a column of hollow fuel pellets instrumented with a central thermocouple and loaded into a recrystallized Zr4 cladding refilled with helium.

The present communication gives a description of the operation conditions which prevailed during the unexpected transient and analyses the recorded data (segment power, fuel central temperature, cooling water temperature, water activity, etc.) are analyzed.

From the data analysis, the pulse initiated from a quasi zero power could be characterized as follows:
- maximum linear heat generation rate (LGHR): 553 W/cm;
- pulse width at half height: 65 s;
- coolant temperature (unpressurized stagnant water): 56-119 °C;
- peak fuel enthalpy at the time of the failure: 215-326 J/g UO₂ (52-78 cal/g UO₂) related to a reference temperature of 20 °C

After the BR2 transient, the experimental device was submitted to a neutron-radiography, which revealed that the fuel segment was broken into 3 fragments and that an important powdered fuel deposit was present at the bottom of the experimental device. The dismantling operations allowed to recuperate the fuel fragments and the deposit which were stored for a further PIE programme.
Table of contents

1. Background
2. Fuel segment characteristics
3. Experimental facilities and irradiation conditions
4. Power history
5. Estimation of the LHGR, central temperature and enthalpy
6. Post-incident observations
7. Future examinations
8. Conclusions

Acknowledgments

References

APPENDIX 1. Main characteristics of the fuel rods and the instrumented segment

APPENDIX 2. Response time of different components and instrumentation of the irradiation rig and reactor

APPENDIX 3. List of abbreviations

FIGURES
1. Background

One of the major objectives of the HBC (High Burnup Chemistry) international programme [1] is to assess the influence of burnup and chemistry on fuel thermal conductivity by direct measurement of the fuel temperature at high burnup.

To reach this objective, instrumented fuel segments, loaded into the PWC (Pressurized Water Capsule - see further), were irradiated in BR2 at different power levels. These segments were previously cut from "mother rods" preirradiated in the PWR type research reactor BR3 at Mol, refabricated by means of the FABRICE process and instrumented with a central W-Re thermocouple at the CEA-Saclay. The mother rod AK199 concerned in this experiment was preirradiated in BR3 up to high burnup (70.3 MWd/kg U peak or 58.4 MWd/kg U mean value). During the start of the irradiation of the segment in BR2, an unexpected reactivity excursion occurred, destroying the fuel segment.

2. Fuel segment characteristics

The fuel column of the segment was 319.4 mm long and was composed of a stack of annular pellets. The segment was instrumented with a high temperature W-Re(5%)-W-Re(26%) thermocouple. The hot junction of the thermocouple was placed in the middle of the pellet situated at the mid-plane of the pellets column.

The main characteristics of the mother rod and the instrumented segment are listed in Appendix 1.

3. Experimental facilities and irradiation conditions

3.1. The BR2 reactor

The BR2 is a high thermal flux MTR (Materials Testing Reactor) [2] moderated by beryllium and light water. The beryllium matrix is composed by 64 channels (84 mm in diameter). Each channel can be loaded with a fuel element or an experimental rig, depending on the experimental programme. The central part of the beryllium matrix loaded with fuel elements composes the core of the reactor. A standard BR2 fuel element consists of several concentric tubular shells made of uranium-aluminium alloy and cladded by aluminium. This fuel design is particularly well adapted to withstand a severe reactivity excursion without damage. The rest of the beryllium constitutes the reflector. The PWC-CCD rig devoted to HBC experiments was loaded into the reflector channel E-330, nearby the core (Fig. 1).
3.2. The PWC-CCD irradiation rig

The PWC (Pressurized Water Capsule) is an instrumented irradiation rig filled with deminerlized stagnant water which can contain a single fuel segment to be tested under steady-state and non-steady-state conditions [2]. The water can be pressurized in the pressure range of 0.05-15.5 MPa (relative) depending on the irradiation programmes (PWR, BWR, thermal conductivity measurement,...). The power of the segment is dissipated radially through the stagnant water towards the BR2 cooling water (Fig. 2) by natural convection with or without boiling.

The PWC is introduced into the CCD (Cycling and Calibration Device) rig which allows to perform the thermal balance of the cooling water, heated by the PWC, using the diaphragm flowmeter and differential temperature measurements [3].

3.3. Irradiation conditions

The PWC-CCD with the fuel segment was loaded in the E-330 irradiation channel situated in the BR2 reflector next to the core. During the experiments the PWC-CCD rig was surrounded by 3 fuel elements.

The calculated unperturbed neutron fluxes in the channel E-330, at the nominal BR2 power of 60 MW, were:

- Thermal neutron flux: \(2.95 \times 10^{18} \text{ n/m}^2\text{s}\)
- Epithermal neutron flux: \(0.176 \times 10^{18} \text{ n/mls}\)
- Fast neutron flux: \(1.84 \times 10^{18} \text{ n/'nr.s}\)

The background nuclear heating was measured by aluminium calorimetric probes in the neighbouring channel H-337 and in the symmetric channel E-30 and by the irradiation of the empty PWC capsule (i.e. without fuel segment) in the channel E-330 before the experiment. Its value was estimated to be 4.8 W/g Al (±0.15 W/g Al) at the nominal BR2 power of 60 MW. Due to the low residual fissile inventory in the high burnup fuel this value gave a non-negligible contribution to the thermal balance compared to the fission power in the segment.

4. Power history

4.1. Power history of the reactor during the cycle 07/93

The cycle 07/93 lasted from 6 October 1993 to 13 November 1993 and was composed of 4 sub-cycles. During these 4 sub-cycles the loading of the BR2 remained nearly unmodified.

Cycle 07/93 A (from 06/10/93 to 25/10/93) was a normal cycle devoted to the operation of the CALLISTO loop for burnup accumulation and to radio-isotope production. The PWC-CCD was not loaded in the reactor.
Cycle 07/93 B (from 30/10/93 to 1/11/93) was a special cycle and was devoted to the measurement of the nuclear heating rate in the empty PWC capsule (i.e. without experimental segment).

Cycle 07/93 C (from 6/11/93 to 8/11/93) was a special cycle devoted to the irradiation of a high burnup instrumented fuel segment similar to the AK199 segment (with about the same burnup but with different fuel characteristics and geometry).

Cycle 07/93 D was planned for irradiation of the AK199 segment. The non-programmed power excursion occurred during the start of cycle 07/93D on 13/11/93. In fact, it was expected to increase the segment power slowly to its nominal value in about 1 day without any risk of cladding failure (according to the COMETHE code calculations performed by BN).

During the sub-cycles 07/93B, C, and D the in-pile sections (IPS) of the CALLISTO loop were loaded with dummy stainless-steel rods.

Fig. 3 represents the power history of the reactor during these sub-cycles.

4.2. Power history of the segment AK199

After the incident, all the instrumentation of the BR2 and the PWC-CCD and the recorded data were analysed in detail to estimate the maximum power reached by the reactor and to calculate the fuel segment power.

Maximum power

The power excursion during the cycle 07/93D was determined by means of the A3 chain of BR2 which measures the activity of \(^{16}\text{N}\) in the reactor cooling water. The registered activity is normally converted into the total thermal power of the reactor. In Fig. 3 the power is presented by a single spike which is too low and equals about 40% of the true value, due to the fact that the A3 chain was not in service during the beginning of the reactivity excursion (one of the major causes of the incident).

Fortunately, the other fission chambers which measured directly the neutron flux in the pool surrounding the core could give information about the reactor power. During the power excursion 2 linear fission chambers (A1 and A2 chains) and 2 logarithmic fission chambers (B1 and B2 chains) were in service. The loading of the reactor was similar during the 4 sub-cycles and these 4 chambers are fixed, so their output signals registered during the power excursion of sub-cycle 07/93D could be converted into power by calibration with data of the other 3 sub-cycles 07/93A, B and C.
The various values of the current and power given by theses chains at the peak of the power excursion were:

A1: \(1.61 \times 10^{-8} \) A or \((70 \pm 4)\) MW or \((117 \pm 7)\) % of the nominal power
A2: \(1.61 \times 10^{-8} \) A or \((68 \pm 4)\) MW or \((113 \pm 7)\) % of the nominal power
B1: \(5.0 \times 10^{-8} \) A or \(69.0\) MW or \(115\) % of the nominal power
B2: \(6.0 \times 10^{-8} \) A or \(64.6\) MW or \(107.5\) % of the nominal power

The nominal power of BR2 for cycle 07/93 D was 60 MW.

These values seem to be reliable and the data obtained with the logarithmic fission chamber B1 were chosen for further analysis because these chamber is situated in the same angular position (300°) as the experimental channel (330°). Fig. 4 presents the signal of the chamber B1 on a semi-logarithmic scale and Fig. 5 represents the converted reactor power on a linear scale.

**Five periods of the power excursion history**

The power excursion can be divided into 5 periods. During these periods, the signals of the BR2 instrumentation were recorded continuously on paper. The signals of the PWC-CCD instrumentation were sampled by the Data Acquisition System (DAS) once a minute.

1st period - criticality

The reactor was critical at quasi zero power of about 0.05 MW (or \(0.0035 \times 10^{-6}\) A). The PWC capsule was filled with unpressurized stagnant water. The temperature of the water in the capsule and the central temperature of the segment were close to the temperature of the primary cooling water of BR2 (about 20°C). The heating rate measured by CCD was negligible. Only the end of this period, which lasted 42 minutes (between 9:05 and 9:47), is represented in Fig. 4.

2nd period - rise to low power

The second period (between 9:47 and 9:52) was a quasi-exponential (linear on a semi-logarithmic scale) increase of the reactor power from 0.05 MW to 3.5 MW (\(0.0035 \times 10^{-6}\) A to \(0.25 \times 10^{-6}\) A). The measured temperatures and the heating rate were increasing too.

3rd period - steady state at low power

During this period, the power of the reactor remained stable at about 3.5 MW (\(0.25 \times 10^{-6}\) A) during 2 minutes. The total heating rate measured by CCD was about 2 kW and represented 10 % of the nominal value for the CCD. This power was sufficiently high and the time of the steady state (2 minutes) sufficiently long compared to the time constant of the CCD to have reliable measurements (Appendix 2).
Furthermore, the values measured by the CCD were analyzed by a new advanced analysis model (CAFIR) developed recently for the PWC-CCD rig data treatment to obtain the Linear Heat Generation Rate (LHGR) of the segment. The LHGR during this stage was 28 W/cm. This stage was used as a calibration reference for adjusting the fuel segment characteristics needed for the temperature and the enthalpy calculations during the power excursion.

4th period - power excursion.

During this period, the reactor diverged nearly exponentially (linearly on a semi-logarithmic scale) from 3.5 MW to 69 MW (0.25×10⁶ A to 5×10⁶ A) with a doubling period of about 1 minute. There was a factor of about 20 between the maximum power and the previous stage. The power excursion lasted 268 s (4.47 minutes). The power increase from 35 MW to 69 MW required about 40 s.

Because the DAS of the CCD was only measuring once a minute, the highest point of the thermal balance is missing. Only the BR2 signals, recorded continuously, are available and the highest values of the LHGR and the central temperature should be extrapolated by calculations. A simplified computer model (FUROT- see further) was created to obtain the LHGR and the central temperature of the segment versus time. This model was validated with the available measurement data of the PWC-CCD experiments. The rest of the analysis was consequently made with the model and no longer with the measured data. Our first estimation of the maximum enthalpy 210 J/g (or 50 cal/g with 20°C reference) of the segment was based on the highest measured central temperature. A better estimation of the maximum enthalpy 326 J/g (or 78.2 cal/g with 20°C reference) has been made based on the calculated central temperature.

5th period - power decrease

The divergence of the reactor was stopped by a manual reverse of the control bars (i.e. insertion of all control bars with electrical motors). The power decreased immediately and practically exponentially (linearly on a semi-logarithmic scale) with a decrease period (factor 0.5) of about 30 s. This power decrease lasted about 2.5 minutes (150 s) to reach a power level lower than 1 MW.

This period was followed by a manual scram (fall of the control bars) when the power was about 1 % (0.6 MW or 0.05×10⁶ A) of the nominal power but that has no importance for the present purpose.

The duration of the power peak at half height (35 MW or 2.5×10⁶ A) was about 65 s.

5. Estimation of the LHGR, central temperature and enthalpy

The fuel segment power (source term) was precalculated with the CAFIR code using expected irradiation conditions for cycle 07/93D and the results of the BN estimation of the fuel segment composition before the experiments. The calculated and experimental values of the average LHGR versus time are presented in Fig. 6. The rather satisfactory agreement between both indicates the validity of the results obtained by calculation. The calculated peak value of the LHGR was 553 W/cm.
The two-dimensional non-stationary computer code FUROT (FUEL Rod Transient), developed at SCK•CEN for on-line modelling of the fuel rod transients during irradiation experiments, was used to estimate maximum fuel temperature and enthalpy.

The thermal conductivity of the irradiated UO$_2$ was determined using formulae recommended in the HBC programme. The value of the thermal resistivity of the gap between fuel pellets and cladding was chosen to obtain the best agreement between the calculated and the measured values of the central temperature under steady-state conditions existing just before the power excursion.

Calculated and experimental values of the central temperature as a function of time are presented in Fig. 7 (The central temperature measurement at 9:55 is missing, because of the disconnection of the thermocouple for control during the power excursion). From the calculations it follows that the maximum central temperature of 2125 K (1852 °C) was achieved in 268 s (4.45 minutes) after the beginning of the power excursion. At this point, the specific enthalpy accumulated in the fuel was 326 J/g UO$_2$ (78.2 cal/g UO$_2$ with 20°C reference). Estimations performed with the FUROT model have shown that cladding rupture could have been caused by thermal expansion of the fuel, about half a minute before the maximum LHGR was reached, at an enthalpy higher than 210 J/g UO$_2$ (50 cal/g UO$_2$ with 20 °C reference).

On the other hand, the heat flux on the segment surface exceeded its critical value during 2 minutes due to the low pressure in the PWC capsule and due to the high magnitude of LHGR (553 W/cm) in the fuel segment during cycle 07/93D. Fig. 8 represents the experimental results of the Onset of Nucleate Boiling (ONB) and the Departure from Nucleate Boiling (DNB) for specific PWC geometry and conditions. The normal path of LHGR versus water pressure for HBC experiments is situated between the ONB and the DNB limits and is also presented there.

One can see that the actual LHGR reaches and exceeds DNB. So, we suspect that some high temperature spots occurred on the cladding surface due to a steam film formation. Examinations of possible changes in the morphology of the Zircalloy cladding should give more information about the temperature reached by the cladding material.

6. Post-incident observations

6.1. Activity release in the PWC capsule

A few minutes after the power excursion, alarms detected a high activity on the rinsing line of the PWC capsule. The rinsing was immediately and automatically stopped; the capsule was isolated. Activity measurements at the top of the PWC capsule and around the rinsing lines showed clearly a cladding failure; the activity decrease by a few decades during the next 2 days indicated the presence of short lived fission products.

Unfortunately there was no on-line fission product laboratory to analyse the rinsing water of the PWC immediately.
The PWC capsule was unloaded from the reactor on 16 November 93. About a month later, on 9 December 93, the first water samples were taken and analysed. Even after dilution, they revealed a high gamma activity; the alpha-contamination of these samples was practically negligible:

\[
{^{134}}\text{Cs activity:} \quad 0.5 \text{ GBq/ml} \\
{^{137}}\text{Cs activity:} \quad 2.5 \text{ GBq/ml} \\
\text{total alpha activity:} \quad 500 \text{ Bq/ml}
\]

Three litres of water of the capsule were progressively diluted and sent to the waste tank. When the activity was sufficiently low, the water was purged out of the capsule with helium to take a neutron-radiography of the segment inside the capsule.

6.2. Neutron-radiography

Schemes of the fuel segment before (Fig. 9a) and after (Fig. 9b) the power excursion have been drawn to facilitate the interpretation of the neutron-radiography (Fig. 10).

The neutron radiography revealed that:
- the segment was broken into 3 fragments;
- the 1st (top) fragment remained hanging in its place at the suspension piece but lost a few pellets at its bottom;
- the second (middle) fragment, about 45 mm long, fell between the 3rd fragment and the basket, and lost a few pellets;
- the 3rd (bottom) fragment fell by about 20 mm (the dilatation and clearance gap) on the bottom of the basket; the central hole of the 3rd fragment was partially filled with debris;
- in total 8 pellets were missing and most of the pellet debris of different sizes fell on the bottom of the basket and the capsule; some of them were hanging at different levels of the basket;
- there was a distance of about 65 mm between the 1st and the 3rd fragment;
- the capsule, the supporting basket and its instrumentation seemed to be intact;
- the central thermocouple of the segment seemed to be intact as well;
- the PWC rig seemed to be recoverable and the integrity of the instrumentation was confirmed by electrical measurements of isolation and continuity at the BR2 pool penetration.

The debris were unloaded from the PWC capsule and the PWC was decontaminated in the hot cell in January 1995. The fragments of the segment and the big debris were recovered. The small size debris and powder were filtered on a glass-wool bed. Everything was stored in a tight container in the hot-cell laboratory for further investigation.

7. Future examinations

A future programme on the debris of the fuel segment should be proposed and carried-out next year. This programme should comprise examinations of the fuel and the cladding and a comparison with the twin rod AK200 previously examined in the framework of the HBC programme.
8. Conclusions

After the power excursion, we observed that the BR2 reactor and especially its fuel elements were not damaged and that they were working normally at all the regimes. The PWC capsule was intact too and the contamination remained under control in the capsule despite the fact that the facility had not been designed for such incident. In January 1995, we successfully unloaded the segment and the debris out of the capsule, which was decontaminated. The micro-grains and the powder were recovered on a glass-wool filter.

We have presented the incident and its consequences in a committee of specialists for nuclear installation safety who estimated that this experience could give useful results. Then the possibility to reproduce such kind of accidents, in a voluntary way, in the framework of LWR safety programmes in BR2 in new water loops specially designed for severe accidents was examined. A feasibility study has started with principle schemes and preliminary calculations. Up to now this study is rather promising.

The fragments and the debris are stored in the hot-cell laboratory and we intend to examine them in detail in 1996. No definitive programme is presently set up and the discussion is open for collaborative work in that field.

Finally, Fig. 11 compares results obtained in pulsed reactors with our results obtained in the BR2 materials testing reactor. One can see that they are in good agreement, and the latter gave a supplementary point for extrapolation to a burnup higher than 70 MWd/kg U.

Acknowledgments

We are grateful to all the persons of SCK-CEN and BELGONUCLEAIRE who have provided us with material and information needed to prepare this paper. In particularly we thank Baudouin ARIEN, Danielle BOULANGER (BN), Willy CLAES and the hot-cell team, Charles DE RAEDT, Albert DELBRASSINE, Claude LAMBIET and the PWC-CCD operation team, Richard LIESENBORGH and the neutron-radiography team, Léon MERTENS (BN), Bernard PONSARD, Frans SCHELLES and the drawing office.
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    24 - 26 September 1986, Karlsruhe - Germany
APPENDIX 1

Main characteristics of the fuel rods and the instrumented segment

The AK199 rod was irradiated during 4 cycles in the BR3 PWR reactor at Mol up to a peak burnup of 70.3 MWd/kg U (mean burnup 58.4 MWd/kg U). It reached the maximum peak power of 319 W/cm during the 1st cycle. Its twin rod AK200 was irradiated in the same assembly during the same cycles up to a burnup of 68 MWd/kg U (mean burnup 56.8 MWd/kg U). The UO₂ fuel pellets with initial enrichment of 7% w/o ²³⁵U were manufactured by the "Société Française d'Eléments Catalytiques" (SFEC), located at Bollène, France. The cladding material was recrystallized Zircalloy-4 tubing fabricated in France by Vallourec. The rods have been initially pressurized with helium at 1.5 MPa. The main design parameters of the mother rods and the instrumented segment are listed in the next table:

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The new geometrical design parameters are listed in the next table:

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</tbody>
</table>
APPENDIX 2  Response time of the different components and instrumentation of the irradiation rig and reactor

Response time of the A1, A2, B1, B2 chains (including the recorder): <1 s.

Integration time of the activity of the $^{16}$N by the A3 chain:
- during the start of the reactor: 8.64 s (1 day/10 000).
- during steady state of the reactor: 86.4 s (1 day/1 000).

Time constants of PWC-CCD device loaded with a fuel segment:
- PWC rig: 21 s.
- CCD calorimeter: 6.4 s.
- Fuel segment central temperature: 31 s.
**APPENDIX 3  List of abbreviations**

<table>
<thead>
<tr>
<th>Abbreviation</th>
<th>Description</th>
</tr>
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<tbody>
<tr>
<td>BN</td>
<td>BELGONUCLEAIRE</td>
</tr>
<tr>
<td>BR2</td>
<td>Belgian Reactor 2: materials testing reactor</td>
</tr>
<tr>
<td>BR3</td>
<td>Belgian Reactor 3: small PWR reactor 10.5 MW electric, shut-down in 1987</td>
</tr>
<tr>
<td>BWR</td>
<td>Boiling Water Reactor</td>
</tr>
<tr>
<td>CAFIR</td>
<td>CALorimetry of Fuel rod in Irradiation Rig</td>
</tr>
<tr>
<td>CALLISTO</td>
<td>CApabiLity for Light water Irradiation in Steady state and Transient Operation</td>
</tr>
<tr>
<td>CCD</td>
<td>Cycling and Calibration Device</td>
</tr>
<tr>
<td>DAS</td>
<td>Data Acquisition System</td>
</tr>
<tr>
<td>DNB</td>
<td>Departure from Nucleate Boiling</td>
</tr>
<tr>
<td>FUROT</td>
<td>FUEL RoD Transient</td>
</tr>
<tr>
<td>HBC</td>
<td>High Burnup Chemistry programme</td>
</tr>
<tr>
<td>MEPPhI</td>
<td>Moscow Engineering Physics Institute</td>
</tr>
<tr>
<td>MTR</td>
<td>Materiel Testing Reactor</td>
</tr>
<tr>
<td>LHGR</td>
<td>Linear Heat Generation Rate</td>
</tr>
<tr>
<td>ONB</td>
<td>Onset of Nucleate Boiling</td>
</tr>
<tr>
<td>PIE</td>
<td>Post Irradiation Examination</td>
</tr>
<tr>
<td>PWC</td>
<td>Pressurized Water Capsule</td>
</tr>
<tr>
<td>PWR</td>
<td>Pressurized Water Reactor</td>
</tr>
<tr>
<td>SCK·CEN</td>
<td>Studie Centrum voor Kernenergie • Centre d'Etudes de l'énergie Nucléaire</td>
</tr>
<tr>
<td>TD</td>
<td>Theoretical Density</td>
</tr>
<tr>
<td>W/g Al</td>
<td>Watt per gram of Aluminium</td>
</tr>
<tr>
<td>W-Re</td>
<td>Tungsten-Rhenium (thermocouple)</td>
</tr>
</tbody>
</table>
Figure 1
BR2 CONFIGURATION

Cross section at reactor mid plane

- FUEL ELEMENT
- CONTROL ROD
- REFLECTOR

Calorimetric probes
PWC-CCD
Figure 2

- ROD: PWR or BWR
- INSTRUMENTED with CENTRAL TE
  \( \phi 9.5 \ldots 13.5 \text{ mm} \)
- \( L \) up to 1m

- PRESSURIZED WATER CAPSULE 0.1–15.5 MPa

- FUEL ROD

- HE3 SCREEN

- BR2 COOLING

- WATER

- CENTRAL THERMOCOUPLE

- FLOW ELEMENT

- \( \text{TE1-1, TE1-2, TE1-3} \)

- \( \text{TE5-1, TE5-2, TE5-3} \)

- \( \text{NFE5-1, NFE5-2, NFE5-3} \)

- \( \text{CCD1-PWC5} \)
FIG. 3: CYCLE 07/93A+B+C+D REACTOR POWER AS A FUNCTION OF ELAPSED TIME
Fig. 4. BR2 power excursion during cycle 07/93D (B1 fission chamber data)
Fig. 5. BR2 power pulse during cycle 07/93D (values calculated on the base of the 07/93C cycle data and B1 fission chamber measurements)
Fig. 6. Calculated and experimental values of the fuel rod power during cycle 07/93D
Fig. 7. Central temperature rise during power excursion
Fig. 8. Heat transfer critical region for PWC rig with AK199 rod
Fig.9a. Fuel pin before power transient (AK199)
Fig. 9b. Fuel pin after power excursion (AK199)
Fig. 11. Peak fuel enthalpy as a function of burnup for reactivity transients in test reactors
Fig. 10. Neutron-radiography of AK199 segment in PWC
STUDSVIK's Experience Related to LWR Fuel Behavior at High Burnup

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Studsvik Nuclear AB, S-611 82 Nyköping, Sweden

1 Introduction

The simulated RIA experiments performed over the years indicate that the failure resistance of the LWR fuel steadily decreases with increasing fuel burnup currently indicating the lowest failure resistance at 50 MWd/kgU, which is the highest burnup level tested so far, see Figure 1 [1].

The fuel degradation affecting the RIA behavior appears to relate in part to gradual changes in the physical properties of the fuel pellets at fuel burnups exceeding 40 MWd/kgU. At about this burnup level the high power density region of the fuel pellets begins to shift towards the pellet periphery concurrently also forming a tiny porous rim zone. The temperature of the peripheral region increases and in the rim zone a characteristic subgrain structure of low thermal conductivity develops incorporating fission gases in tiny closed pores [2]. In a severe RIA test the irradiation embrittled fuel cladding initially expands extremely rapidly as a result of fuel pellet thermal expansion and grain separation. Fission gases are released that promptly build up high pressures within the fuel structure itself, in the rim zone, and exert an impact loading on the cladding. As a result the Zircaloy cladding expands and may fracture at several locations around the circumference. Fuel particles might be ejected into the coolant water [3]. Strong fuel-to-clad bonding is likely to have developed under the long term irradiation exposure to high burnup. The bonding consists probably of a brittle ceramic layer produced in a chemical reaction between Zr and UO₂. Under such conditions clad crack initiation is facilitated and multiple clad cracking is expected to occur.

However, LWR fuel designed for extended high burnup differs from fuel now being operated up to current high burnup levels (<50 MWd/kgU) in various technical aspects which may have important impacts on fuel behavior even at lower burnup levels. In particular, the fuel enrichment is higher, perhaps up to 5 %, and higher contents and sometimes new types of burnable poisons are incorporated. Also, new fuel design concepts and new material selections and combinations may be involved, specific for the particular fuel manufacturer.

The higher fuel enrichment combined with the poisoning effect of the burnable absorber permits the fuel to be exposed to a higher burnup level at the same linear heat rating (LHR) as with the previous lower enrichment. This means a prolonged operation at a higher fuel pellet temperature than earlier because of gradual loss of thermal conductivity. This causes additional generation and release of fission products (FP) and higher fuel swelling from FP...
pore formation, all of which can affect the integral fuel rod behavior, for example pellet clad interaction (PCI) and thermal feedback. Above approximately 40 MWd/kgU a rim zone of higher local burnup develops, that grows radially with increasing fuel burnup in a peripheral pellet zone of rising temperature under radial expansion.

The fuel cladding will be exposed to a higher dose of fast neutrons and becomes more embrittled as a consequence of the burnup extension. In parallel, the corrosion process proceeds for a longer time and at a rising clad metal temperature, which causes an increasing surface oxidation and an increasing hydrogen pickup fraction. The hydrides may preferentially precipitate at the clad outside surface, where they occasionally oxidize and split off.

As a consequence the integral fuel rod behavior will also be affected. Over the time the pellet-to-clad mechanical interaction (PCMI) will be intensified because of fuel swelling causing a gap closure and a general straining of the cladding. At weak spots this may result in a clad failure on up-ramping. The potential for local pellet/clad bonding increases with the appearance of the rim zone, which is associated with a temperature rise at the pellet surface and a fuel pellet expansion.

It can be speculated what the impact of a sudden power rise at a high strain rate might have been on the integral behavior of fuel designed for high burnup. Within the burnup range 40-50 MWd/kgU, where still substantial enriched uranium remains for driving the power rise, detrimental consequences seem most likely to appear. Assuming that the PCI/SCC failure mechanism still operates at this burnup level, the conditions for its initiation seem favorable. There are intense PCMI conditions prevailing and the clad might be locally bonded to the cladding. Hence, on a power ramp the pellets will expand and strain the cladding at positions of stress concentrations like pellet crack openings and bonding cracks, which might induce critical stress levels for SCC. Concurrently FP are released to the inside surface of the cladding. On account of the higher irradiation damage of the cladding, the presence of hydrides, bonded areas and the numerous opening cracks at the surface of the fuel pellets clad fractures might be more numerous and destructive than experienced at lower burnup levels. The release of FP to the coolant might also be faster and larger. However, it can be argued that the presence of the steep fuel temperature gradient as well as the porous rim zone at the pellet periphery surface might act to reduce the pellet crack openings to become insignificant as stress risers. Only well-designed power transient experiments will resolve the question.

For obvious reasons normal and off-normal power transients do not present similar concerns as regards safety for the performance of high burnup fuel as a RIA will do. The PCI/SCC failure propensity has been extensively explored in the past over the whole burnup scale but essentially up to 40 MWd/kgU with only a few tests up to 50 MWd/kgU. The performance limits have been
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quite well defined. Some deficiencies in performance seem, however, to relate to fast off-normal power transients as will be reported in this paper.

In view of the commercial interest in extending the LWR fuel discharge burnup well beyond 50 MWd/kgU it seems urgent to also examine to what extent a progressive degradation of the fuel integrity or other phenomena of relevance are reflected in the many well-designed power ramp tests over the whole burnup range that have been performed in the past. In particular, ramp tests performed under the specific conditions of "normal" (operational) and "off-normal" ramp rates attract interest. STUDSVIK NUCLEAR has some 25 years of experience within this area to refer back to.

2 STUDSVIK's Power Ramp Experience of Relevance to High Fuel Burnup

STUDSVIK's power ramp experience traces long back in time to the late 1960's when some exploratory ramp tests were performed at the R2 reactor in an attempt to investigate the possible reason why a STUDSVIK test fuel assembly became leaking during base irradiation in the Halden HWR. Among others a few ramp tests were performed at the R2 reactor on some test fuel of various low burnup levels (below 20 MWd/kgU). During the course of the power ramp testing it became apparent that some test fuel failed due to pellet/clad interaction at highly strained positions like circumferential ridges, occasionally also producing deformation patterns "X-marks" on the clad outside. Incipient clad cracks were seen to initiate just opposite pellet crack openings, see Figure 2. A fairly consistent failure boundary as function of burnup became apparent [4]. These experimental findings formed the basis for the series of power ramp studies that later followed and actually now is stretching into the high fuel burnup regime.

Thus, since the early 1970's, a long series of domestic, bilateral and international fuel R&D projects, primarily addressing the PCI/SCC failure phenomenon have been conducted under the management of STUDSVIK NUCLEAR and its predecessors. These projects have been pursued under the sponsorship of different organizations: The bilateral projects mainly by fuel vendors and the international projects by different groups of fuel vendors, nuclear power utilities, national R&D organizations and, in some cases, licensing authorities in Europe, Japan and the U.S.A. In most of the projects the clad failure occurrence was studied under power ramp conditions utilizing the special ramp test facilities of the R2 test reactor. The current projects are not limited to PCI/SCC studies but some of them also include other aspects of fuel performance, to be discussed below. During the late 1970's and 1980's the series of international PCI ramp projects branched out in two directions. One series was initially concentrated on the PCI phenomena and fuel modeling under normal operational conditions for different types of
BWR and PWR fuel rods subjected to increased burnup. These projects were in a broad sense aimed at decreasing the fuel costs by increases in fuel utilization and reactor availability. Most of the bilateral fuel projects fell into this category. The other series of international projects concentrated on more safety-oriented issues, aimed at providing data for fuel-related safety considerations.

In later years two other types of fuel R&D projects have been introduced: End-of-life rod overpressure studies and defect fuel degradation experiments. The end-of-life overpressure projects were designated ROPE I and ROPE II (Rod OverPressure Experiments). The defect fuel degradation projects are designated DEFEX I and DEFEX II. Currently other types of projects are also under discussion.

2.1 Operational Ramp Failure Resistance Studies

The first international project, the BWR INTER-RAMP (1 h) project (executed during 1975-79), produced a very consistent set of ramp test results, that still serve as a basis for checking fuel models, see Figure 3 [5]. No such systematic tests have been performed at higher burnups. The ramp failure threshold was located at 42 kW/m. It might be of interest to note that the time to failure occurrence decreases with burnup accumulation, by a factor of about 10 in passing from 10 to 20 MWd/kgU. PCI/SCC cracks were seen to initiate also at the edges of ZrO\(_2\) patches formed on the clad inside surface at positions of strong PCMI.

The results from additional BWR ramp test programs i.e. within the SUPER-RAMP and SUPER-RAMP II projects, indicate that the ramp failure threshold tends to decrease continuously over the whole burnup range, down to below 35 kW/m at fuel burnup levels of approximately 35 MWd/kgU, see Figure 4 [6, 7]. The ramp test data of 9x9 type fuel do not differ from the 8x8 type fuel at the burnup levels tested. However, such slimmer fuel will during service operate with a wider failure margin. The use of Zr liner cladding substantially improves the PCI failure resistance on account of lower imposed stress levels at strained clad positions facing an opening UO\(_2\) crack such as shown in Figure 1. The improvement over the whole burnup range up to 50 MWd/kgU is apparent from Figure 5, which is partly based on STUDSVIK bilateral ramp test data [8]. A minimum in failure resistance appears but still at an acceptable high power level at about 30 MWd/kgU.

The SUPER-RAMP PWR ramp tests data present a split picture in failure resistance, see Figure 6. Some fuel rods survived without failures up to the highest burnup level tests, 45 MWd/kgU. Other fuels of older origin failed at lower levels, approximately at 37 kW/m in the burnup range of 35 to 40 MWd/kgU. Later ramp test programs, partly based on STUDSVIK data present a consistent burnup dependence, see for example Figure 7 [9].
Again, there is an indication of a minimum of failure resistance in the medium burnup range of approximately 35 MWd/kgU and no failure indications beyond 40 MWd/kgU in burnup.

A special feature of potential interest at high fuel burnup is the large clad straining (>1 %) that is observed among others on ramping at 45 MWd/kgU, see Figure 6, the test point at 49 kW/m. The question is whether on return to the pre-ramp LHR a pellet/clad gap will appear, which on continued operation will not close but remain and possibly widen and initiate a clad lift-off phenomenon.

As stated a common feature of the operational power ramp studies for both BWR and PWR type fuels is the appearance of a minimum of failure resistance in the medium burnup range, 30-35 MWd/kgU, however, still at an acceptable high level. Also a common feature is the disappearance of the PCI/SCC failure occurrences at higher burnups above approximately 40 MWd/kgU. It may be of interest to note that the PCI/SCC failure disappearance within the burnup range of around 40 MWd/kgU is coincident with the first appearance of the rim zone. For fuel designed for burnups beyond 50 MWd/kgU it is not self-evident that such a favourable PCI behavior will be reproduced. Prolonged operation at a steady high power might affect the failure behavior unfavorably. The closure of the pellet/clad gap will affect the fuel clad straining as indeed any formation of extensive pellet-to-clad bonding, particularly in PWR fuel that operates with high differential overpressures. Accordingly ramp test experiments performed in the past at extended high burnup on LWR fuel designed for lower burnup targets may not be representative for fuel intentionally designed for extended high burnup.

2.2 Safety-Oriented (Off-Normal) Ramp Resistance Studies

In a number of the international fuel research projects (see below) it was demonstrated by means of power transient tests (intentionally interrupted power ramp tests) that when LWR test fuel rods were exposed to off-normal overpower ramps of increased severity, they exhibited a regular PCI failure progression above the failure threshold. A higher transient peak power level (>40 kW/m) resulted in an earlier fission product leakage from the fuel rods. Stress corrosion cracks initiated promptly on fast upramping, i.e. within the order of seconds, and penetrated the cladding wall within about a minute. Depending on the actual power "over-shoot" and the time spent above the failure threshold, the transient passed consecutively through a number of power-time regions defining the progressive steps of the failure process, see Figure 8.

Some of the LWR fault transients of the types that might be expected to occur once in a reactor year or once in a reactor lifetime carry a potential for
causing PCI fuel clad damage or failure (i.e. through-wall crack penetration) on surpassing the PCI failure threshold. A question of prime concern is then whether a fast single transient of the type mentioned, occurring in a power reactor, will result in fuel failures due to PCI, eventually followed by a release of radioactivity to the coolant. The burnup dependence of these types of fast transients has not been investigated so far but may shift in severity depending on the effectiveness of the PCI/SCC failure phenomenon, on pellet/clad bonding, etc.

In the INTER-RAMP Project, BWR fuel rods were subjected to power transients of varying "over-power" levels beyond the PCI failure threshold, where cladding failure and fission product release occur after a sufficient hold time (see Figure 3). The results demonstrated a systematic time dependence of the fission product release to the coolant from the failed fuel rods [5]. An increase in the power "over-shoot" of 5 kW/m caused a decrease of the time to fission product release by a factor of about 10 at a burnup of 10 MWd/kgU.

In the DEMO-RAMP II (DRII) Project BWR fuel rods of intermediate burnup levels, 20 MWd/kgU, were subjected to intentionally interrupted short-time power transients at linear heat ratings a few kW/m above the PCI failure threshold [10]. No cladding failures were detected after the transients but a large number of non-penetrating (incipient) cracks were observed. They had formed very rapidly, within a minute. These cracks could be observed by destructive post-irradiation examinations only. The crack depths ranged from 10 to 60 percent of the cladding wall thickness, see Figure 9.

In the TRANS-RAMP I (TRI) Project, BWR fuel rods of similar intermediate burnup levels were subjected to simulated short time power reactor transients in a wide range of "over-powers" but at characteristic very fast ramp rates, in the range of 5000 W/[cm-min] [11]. The test results were similar in principle to the DR II results and permitted a tentative interpretation of the PCI failure progression in terms of well-separated power/time boundaries defining 1) crack initiation at the inside surface of the cladding, 2) through-wall crack penetration and 3) leakage of fission products to the coolant water, see Figure 10. The time to FP release was quite long, in the order of 10 minutes.

In the TRANS-RAMP II (TRII) Project, PWR fuel rods of higher burnups, approximately 30 MWd/kgU, were subjected to short power transients, with ramp rates within the range of 5000 W/[cm-min] corresponding to a steam line break event in PWRs [12]. The PCI failure progression diagram obtained turned out to be quite similar to the one obtained from the TRI project for BWR fuel rods, indicating comparable very short times to failure above 50 kW/m, i.e. approximately one minute, see Figure 11. However, the FP release was imminent in this case.
The crack initiation and penetration processes can only be detected by special hot cell laboratory or test reactor techniques. The delay of the fission product release indicates that in power reactors cladding cracks that form during fast transients and terminate before any outleakage of fission products, may remain undetected until manifested in later operational manoeuvres.

In an attempt to better understand the PCI/SCC failure mechanism and possibly improve the failure propensity of 8x8 BWR type fuel, in particular during off-normal transients a few power ramp tests were performed in an in-house R&D project on fuel of a special design, using "rifled" cladding [13]. The results were remarkably good and surprising, see Figure 12. No failures occurred in the two separate tests executed at 10 MWd/kg after holding for 12 hrs at the peak power level of 58-60 kW/m. One test was of the operational type, the other one of the off-normal transient type. The later power ramped fuel that remained intact was base irradiated still further up to 20 MWd/kgU where the same fast transient test was repeated, again without failure. In the rifled clad fuel being tested, see Figure 13, the UO2 pellets were coated with a 5 μm thick graphite layer, while, intentionally, there was no coating applied to the clad inside surface. The idea was to check if active carbon during the course of the irradiation exposure would form and precipitate on the clad inside surface and act as a getter for SCC active fission products. Active carbon would form as a result of the expected reaction 2 CO → C + CO2, where CO continuously serves as a transport medium of C from the hot graphite on the pellets to the low temperature clad wall, where amorphous active carbon might steadily precipitate. CO forms continuously during operation as graphite reacts with oxygen sources within the system. The CO2 formed at the clad surface diffuses back and reacts with the graphite to form CO again, all in a cyclic scheme. Apparently the ramp test results reveal that the PCI/SCC process did not come into operation in spite of the repeated severe test conditions.

This approach in fuel design might be attractive at very high fuel burnup, because the graphite coating on the fuel pellets and the active carbon layer on the clad inside, separate or in combination, will act to prevent the fuel pellets from chemically reacting with the Zircaloy cladding to forming a ceramic bond during the long time irradiation exposure. In case of a RIA the presence of such an interfacial layer will inhibit or delay clad/crack initiation. The cladding will sustain higher pellet temperatures and tolerate more fuel expansion and straining before any failure. The presence of the large number of tiny axial channels in the pellet/clad interface around the circumference (see Figure 13) will also help to reduce the high fission gas pressure build up. In addition, the particular pattern of rifling of the cladding will act to produce a mechanically well defined stress pattern on pellet expansion that induces a quite uniform straining of the cladding. According to test results [13] and stress analysis [14] the PCI/SCC failure probability will be reduced effectively also in the absence of a graphite coating.
3 Studies of Clad "Lift-Off" Phenomena

When LWR fuel is used at increasingly higher burnups the question of how the fuel might behave when the end-of-life rod internal pressure becomes greater than the system pressure attracts a considerable interest.

On one hand end-of-life overpressure might lead to clad outward creep and an increased pellet-clad gap with consequent feedback in the form of increased fuel temperature, further fission gas release, further increases in overpressure etc. On the other hand increased fuel swelling might offset this mechanism. In connection with such considerations STUDSVIK NUCLEAR initiated two international Rod OverPressure Experiments (the ROPE I and ROPE II projects).

The purpose of the first of the international project, ROPE I, was to investigate the behavior of BWR 8x8 fuel rods, irradiated in the Ringhals 1 reactor to a burnup of about 35 MWd/kgU [15]. The rods were refabricated and pressurized to give hot internal overpressures during R2 irradiations of approximately 0, 4 and 14 MPa, respectively. The clad creepout and the time dependent changes in fuel rod conductance were investigated as functions of rod overpressure. The rod with the highest overpressure had a measured diametral cladding outward creep strain of 11 \( \mu \)m after 1634 hours irradiation, with no apparent primary creep. This exceeded the expected pellet diameter increase attributable to fuel matrix swelling, since the average swelling rate measured in the fuel would only have resulted in a pellet diameter increase of 3.2 \( \mu \)m after 1634 hours. Thus it was successfully demonstrated that a BWR fuel rod with an internal overpressure in excess of the pressure causing a cladding creepout rate as fast as the fuel solid swelling rate, can be operated at a LHR of up to 22 kW/m for more than 2 months without any apparent detrimental effect.

In the second project, ROPE II, PWR fuel is being investigated in the same manner. Data from this project have not yet been reported.

4 Some Concluding Remarks

The ramp test experience gained over the years at Studsvik appears in several aspects to be of direct relevance to the behavior of LWR fuel at high burnups.

The operational type power ramp rates cease to cause PCI/SCC failures beyond an approximate burnup level of 40 MWd/kgU for fuel designed for a discharge burnup of approximately 50 MWd/kgU. The disappearance of the failure propensity seems to coincide with the appearance of the rim zone in the fuel pellets.
In contrast, the off-normal power ramp rates promptly produce PCI/SCC failures at "over-powers" (above the failure threshold) within the medium burnup range tested in both BWR and PWR fuels. In BWR fuel the release of FP is delayed several minutes after the failure event while the release is imminent in PWR fuel.

However, fuel designed for extended high burnups may retain a failure propensity under operational type ramp rates beyond 40 MWd/kgU because of the prolonged operation at the initial high LHR, which will intensify the PCMI conditions. The appearance of a ceramic fuel/clad bonding may have a failure promoting impact, even in the absence of the PCI/SCC failure phenomenon.

As interfacial areas of pellet/clad bonding are likely to become more numerous and wider at extended high burnup levels, it can be expected that the ramp failure resistance will be affected, in particular at high power ramp rates. In view of this concern it appears advisable to closer investigate this phenomenon.

In this connection STUDSVIK has considered the application of a thin graphite coating on the fuel pellets in combination with a properly "rifled" cladding. Under RIA conditions such measures would be beneficial as the graphite coating would eliminate any pellet/clad bonding that otherwise would cause an imminent crack initiation of the fuel cladding. Instead, the destructive impact of the clad straining and the FG pressure loading of the cladding will be better controlled and reduced. In addition, the test results indicate, though few in number, that the PCI/SCC failure propensity would be eliminated over the whole burnup scale at both normal and off-normal power ramp rates.

The clad "lift-off" studies indicate that BWR fuel with an internal overpressure in excess of the pressure causing a cladding creep out rate as fast as the fuel solid swelling rate, can be operated at 22 kW/m for more than 2 months without any apparent detriment effect.

A possible concern at high fuel burnup might be the substantial fuel rod swelling of 1 % or more that might follow high power transients. The question is whether the pellet/clad gap will close on continued operation or remain open and initiate a clad lift-off process.
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Figure 1
Peak Fuel Enthalpy Versus Burnup for Simulated RIA's in Test Reactors [1].

Figure 2
Incipient Crack in Zircaloy Cladding Facing Crack Opening in UO₂ Pellet x 10 [4].
Figure 3
INTER-RAMP Data - Power Increase above Average of Last Low Power Pre-Ramp Irradiation Period vs. Time to Fission Product Detection [5].

Figure 4
Comparison of 9x9 Ramp Data with Data from other Ramp Programs [7].
Figure 5
Ramp Test Results from NFD-Hitachi-Toshiba [8].

Figure 6
SUPER-RAMP Data - PWR Subprogram. Fast Ramp Test Results. Ramp Terminal Level vs. Burnup [6].
Figure 7
Power Ramp Performance of the Radial Textured Cladding and High Content Gadolina Fuel Comparing with the Threshold of PCI Failure of the Standard Fuel [9].

Figure 8
Schematic PCI Failure Progression Diagram.
Figure 9
DEMO-RAMP II Data - Variation of Crack Depth (Percent of Wall Thickness) with Ramp Terminal Level and Time at Power above 30 kW/m [10].

Figure 10
Figure 11
TRANS-RAMP II Data - PCI Failure Progression for PWR Fuel [12].

Figure 12
Transient Performance of Improved Fuel Design Using "Rifled" Clad/Graphite Coated Fuel Pellets (BWR).
Very shallow, axially extending channels are formed in the space between pellets and cladding.

Figure 13
Schematic Cross Sections of "Rifled" Clad Fuel [13].
Appendix

Recent Work on "Ultra-Fast" Ramps in the R2 Test Reactor

M Grounes, K Malén, E B Jonsson and M Carlsson

1 Introduction

As mentioned in the main paper a few recent simulated RIA experiments (Reactivity Initiated Accidents) with high burnup fuel (55 and 65 MWD/t) have focused interest on ANSI Class IV events. STUDSVIK is developing a new type of "ultra-fast" ramps, faster than the fast ramps performed in earlier safety-related projects, such as the TRANS-RAMP I and TRANS-RAMP II projects [1,2], but slower than the simulated RIA experiments performed in special, dedicated test reactors. These new "ultra-fast" ramps could reach e.g. 100 kW/m during a 1 sec effective ramp time, corresponding to an enthalpy increase of 45 cal/g.

According to this concept a number of high burnup fuel rods of BWR or PWR types, or both, would be exposed to "ultra-fast" ramp rates to preselected enthalpy increases in the R2 reactor. The main objective is to identify any adverse or inadequate fuel rod behavior, for example abnormal fuel rod swelling, fast failure of the cladding, fast release of fission products, loss of fuel integrity on clad fracturing causing dispersion of fuel particles in the coolant water, etc. Detailed non-destructive and destructive examinations (including advanced types of ceramography) would follow.

In the technical program developed the power ramp behavior and failure propensity and failure mechanisms of commercial LWR fuel are studied using "ultra-fast" power ramp rates (more than 60000 W/[cm-min]) at high fuel burnup. The main objective is to produce ramp test data in the burnup range of 30-50 MWd/kgU for current commercial type of LWR fuel designed for ultra high burnup.

The work will be performed within the framework of an international fuel R&D project, organized by STUDSVIK NUCLEAR, the ULTRA-RAMP project.
2 Testing Technique

2.1 Background

The test rod ramping facility in the R2 test reactor has so far been used for slow and intermediate fast power ramps simulating power transients in BWR and PWR power reactors. A feasibility study, pursued during 1994-95, has shown the possibility to extend the ramping range to higher peak power levels in very fast transients. The aim is to simulate some power reactor transients of the RIA type.

To accomplish this task within the safety requirement of the R2 test reactor a different technique has to be used than in earlier tests. The peak power is achieved in a special flux trap position with high thermal neutron flux and the increased ramp speed will be achieved by a mechanical device for rapid transfer of the test rod in and out of the test position.

The studies made up till now show that high burnup test rods can be ramped to an enthalpy increase of more than 30 cal/g with a transient time of one second.

2.2 RIA Transients

Simulations of RIA transients are by tradition made in special dedicated power burst reactors of the TRIGA type where very short and large energy pulses are generated in the test sample within e.g. 10 milliseconds. The energy pulse for fresh fuel can reach 200 to 300 cal/g without failure of the cladding. However, recent RIA transients of this type of high-burnup fuel rods have resulted in cladding failures at as low energy pulses as 30 cal/g [3, 4].

The mechanisms for the failure are not fully known but one mechanism can be the rapid expansion of the pellet due to the temperature increase of the fuel. Another cause for failure can be the rapid gasification of the fuel at the elevated temperature obtained due to the energy pulses. There are unresolved questions such as: What will happen with the large amount of small high pressure fission gas bubbles existing in high-burnup fuel and what influence has the rim zone which exists in high-burnup fuel?
2.3 Power Levels in the R2 Flux Trap Facility

An MTR type reactor typically gives high neutron fluxes due to its compact design with a high power density in the core. The thermal neutron flux can be further increased by over-moderation in a flux trap in the test rig.

This method gives a test position in the reactor with a thermal flux level of more than $4 \times 10^{14}$ n/[cm$^2$-s], which is sufficient to give a PWR test rod (with a burnup of 45 MWd/kgU) a steady maximal LHR of over 100 kW/m. The flux trap also functions as a stabilizing tool for the test position, resulting in a reactivity balance for the rod in - out of less than 50 pcm.

Achievable power levels in the R2 test reactor have been studied with the CASMO code [5]. Typical PWR 17x17 rods and BWR 8x8 rods have been assumed to be exposed to over 50 MWd/kgU in their respective power reactor environment. CASMO calculations were then performed of the steady state power level for the rods in the R2 test rig. The radial power distribution in the test rod, which depends on the burnup, is considered to have an influence on the test rod failure mode. The radial exposure distribution in the test rod was calculated with the CASMO code and subsequently used in the evaluation of the test rod maximum LHR (Linear Heat Rate).

The maximum LHR in high-burnup test rods to be tested in the R2 flux trap facility, calculated with the CASMO code, is shown in the following table.

<table>
<thead>
<tr>
<th>Rod type</th>
<th>Initial enrichment w/o</th>
<th>Burnup MWd/kgU</th>
<th>Max. LHR kW/m</th>
<th>Energy deposition during 1 sec cal/g</th>
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<tr>
<td>PWR 17x17 rod (5.44 g/cm)</td>
<td>4.5</td>
<td>40</td>
<td>106</td>
<td>47</td>
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<tr>
<td></td>
<td></td>
<td>45</td>
<td>101</td>
<td>44</td>
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<td></td>
<td></td>
<td>50</td>
<td>96</td>
<td>42</td>
</tr>
<tr>
<td></td>
<td></td>
<td>55</td>
<td>91</td>
<td>40</td>
</tr>
<tr>
<td>BWR 8x8 rod (9.02 g/cm)</td>
<td>4.0</td>
<td>35</td>
<td>193</td>
<td>51</td>
</tr>
<tr>
<td></td>
<td></td>
<td>40</td>
<td>186</td>
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<tr>
<td></td>
<td></td>
<td>45</td>
<td>179</td>
<td>47</td>
</tr>
</tbody>
</table>
2.4 Testing Technique

Small test rodlets of about 10 cm length refabricated by the STUDFAB technique, are to be tested in the R2 flux trap facility. The test rod will be pushed from a non-flux position to a high-flux position by a mechanical device, achieving calculated power levels in the high flux position mentioned in Section 2.3. The following measurements of interest can be performed during the experiment: Elongation measurements, internal pressure and diametral changes. The energy deposition is controlled by the time the test rod is located in the high-flux position in the flux-trap facility.

Different operational parameters can be selected by choice of temperature (from cold to operational temperature), pressure (from low to operational pressure) and power (from zero to operational power).

2.5 Fuel Modeling

The conditions in the R2 flux trap facility are obviously not identical either to those in a power reactor during a RIA event or to those in a pulse-type reactor. Recently it has also been discussed whether the conditions in the two last-mentioned cases really are equivalent. In order to investigate these questions further STUDSVIK NUCLEAR has performed a series of fuel modeling calculations.

The basis for the calculations has been two different fuel rods as follows:

- A BWR 8x8 type rod, with a burnup of 45 MWd/kgU and exposed to an enthalpy increase of 75 cal/g in three different "cold" (50 °C) cases. The rim zone has been assumed to be 0.1 mm thick.

- A PWR 17x17 type rod, with a burnup of 45 MWd/kgU and exposed to an enthalpy increase of 66 cal/g in one "hot" (280 °C) case. It should be noted that this type of fuel rod has nearly the same dimensions as a BWR 10x10 type rod.

The three different cases which the fuel rods have been exposed to are

- an assumed RIA transient in a power reactor (BWR only)
- an "ultra-fast" ramp in the R2 flux trap facility (both BWR and PWR)
- an assumed RIA-simulating transient in a pulse-type test reactor (BWR only).

The results of CASMO code calculations, showing a comparison between the operational radial power distribution in a PWR at low and high burnup and the radial power distribution in a high-burnup test fuel rod in the R2 flux
The corresponding BWR data are shown in Figure 2. As can be seen the 45 MWd/kgU curves for the power reactor and the R2 test are very similar for both types of fuel.

A comparison of the three different cases mentioned above (RIA in power reactor, "ultra-fast" ramp in the R2 flux trap facility and RIA-simulating test in a pulse-type reactor) for the BWR rod is shown in Figures 3-5. Data for these cases can be summarized as follows.

<table>
<thead>
<tr>
<th></th>
<th>Assumed RIA in power reactor</th>
<th>&quot;Ultra-fast&quot; ramp in R2 flux trap</th>
<th>Assumed RIA simulation in pulse reactor</th>
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<tr>
<td>Energy Deposition, cal/g</td>
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<td>75</td>
<td>75</td>
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<tr>
<td>Ramp Speed, time to RTL</td>
<td>0.01 sec</td>
<td>0.1 sec</td>
<td>1 msec</td>
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<td>Ramp Terminal Level, kW/m</td>
<td>1800</td>
<td>180</td>
<td>18000</td>
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<td>Ramp Duration</td>
<td>0.15 sec</td>
<td>1.5 sec</td>
<td>15 msec</td>
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<tr>
<td>Max SHF, kW/m², at time t (in seconds)</td>
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<td>1600/2</td>
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<tr>
<td>Max Temp, pellet surface, °C</td>
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<td>450</td>
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<td>1100</td>
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</tbody>
</table>

The corresponding curve for an "ultra-fast" ramp of the PWR rod in the R2 flux trap facility is shown in Figure 6.
3 References

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Figure 1
PWR Rod Radial Power Distribution

Figure 2
BWR Rod Radial Power Distribution
Figure 3
Assumed RIA in Power Reactor - BWR

Figure 4
"Ultra-Fast" Ramp in R2 Flux Trap - BWR
Figure 5
Assumed RIA Simulation in Pulse Reactor - BWR

Figure 6
"Ultra-Fast" Ramp in R2 Flux Trap - PWR
Session III: PLANT CALCULATIONS

Co-Chair: S. LANGENBUCH and K. VALTONEN

Summary of the Session ............................................................................................................. 243

« A Best-Estimate Assessment of Rod Ejection Fuel Duty in PWRs »
S. RAY, D.H. RISHER, B.J. JOHANSEN, M.J. HONE, A. HOLLAND,
U. BACHRACH, G.E. DERYLO ........................................................................................................ 245

« Study of the Rod Ejection Transient on a PWR Related to High Burnup
Fuel Rupture Risk »
S. STELLETTA, M. MOREAU ........................................................................................................... 251

« On the Role of Burnup Effects of Fuel Properties in RIA Analysis »
R. KYRKI-RAJAMAKI .................................................................................................................. 269

« Analysis of the Fuel Behaviour under Rod Ejection Accident in the PWR »
C.B. LEE, B.O. CHO, O.H. KIM, Y.B. KIM, K.S. SEO, J.G. CHUNG,
C.C. LEE ....................................................................................................................................... 279

« Realistic Scoping Study of Reactivity Insertion Accidents for a
Typical PWR and BWR Core »
L.J. AGEE, A.F. DIAS, L.D. EISENHART, R.E. ENGEL .................................................................. 291

« Methodology and Results of RIA Studies at Siemens »
D. BENDER, H. BAUER, F. WEHLE, D. KREUTER, H. FINNEMANN,
J.L. MARYOTT, R.A. COPELAND .................................................................................................. 305

« Analyzing the BWR Rod Drop Accident in High-Burnup Cores »
D.J. DIAMOND, L. NEYMOTIN, P. KOHUT .................................................................................... 315

« Investigations Related to Increased Safety Requirements for Reactivity
Initiated Accidents »
F. HOLZGREWE, J.M. KALLFELZ, M.A. ZIMMERMANN,
C. MAEDER, U. SCHMOCKER ........................................................................................................ 335

« Analyses of Rod Drop Accidents Using a Three-Dimensional Transient
Code for Reactivity-Initiated Events of Boiling Water Reactors »
T. OTA, T. ANEGAWA, A. OMOTO, T. OTA, M. NAGANO,
S. IZUTSU ........................................................................................................................................ 353

« Realistic Evaluation of Reactivity Insertion Accidents in Boiling Water
Reactors »
C.L. HECK, R.A. RAND, G.A. POTTS, J.K. KLAPPROTH,
R. HARRINGTON, J.G.M. ANDERSEN ......................................................................................... 377
SUMMARY OF SESSION III : PLANT CALCULATIONS

For BWRs and PWRs, the design basis RIAs have been reviewed to determine the effect of the presence of high burnup fuel on the accident consequences. Results of analyses for rod ejection accident (PWR) and control rod drop accident (CRDA) (BWR) have been presented. The main objective was to perform more realistic evaluations and to compare results with licensing approaches. Realistic evaluations start from the specification of core loadings of operating plants and use 3D neutron kinetics methods. Additional conservative assumptions were taken for some analyses presented: these included e.g. conservative assumptions on initial conditions of the plant and sensitive parameters such as rod worth, peaking factors, feedback mechanisms, etc.

The comparison of calculations by refined 3D neutronics and thermohydraulic models and licensing approaches using zeroD- or 1D-neutronics shows a reduction of enthalpy rise of approximately a factor of 2 to 3. For many PWRs, the control rod worth is below prompt-critical conditions under realistic assumptions. For BWRs results were presented which show that the reactivity insertion of CRDA reaches prompt-critical values of 1.3$ to 1.5$. Higher values such as 2 to 3$ may be reached under conservative assumptions.

The most important parameter for RIA analyses is the inserted reactivity by the control rod. The consequences are localised to the nearest fuel assemblies of the dropping or ejecting control rod, typically 2 to 3 rows. The core loading pattern influences the results in two aspects:

- it affects the control rod reactivity worth
- it determines the location of fresh and burnt fuel assemblies in the neighbourhood of the dropping or ejecting rod.

The dependence of enthalpy rise on burnup is evaluated in most contributions by detailed scatter plots. Generally, these figures show a decrease of enthalpy rise with increase of burnup. Analyses of RIA for both reactor types were presented which showed that based on realistic evaluations, the number of failed rods was small or even zero. In some contributions, preliminary burnup-dependent fuel failure limits have been used, including CABRI-REP Na1 test, to determine the number of failed fuel rods during RIA.

Questions remain as how to systematically consider uncertainties in sensitive parameters and issues like the effect of burnup on fuel rod parameters (e.g. heat conductivity and capacity, gap resistance and radial power distribution in the fuel rod).

A controversial discussion is about the importance of the RIM-effect on the fuel rod failure mechanism. Some experts argue that thermal expansion is sufficient to explain fuel rod responses without assuming additional fission gas release from the RIA area.

Based on the comparison of reactor transient time-behaviour and the pulse width of RIA experiments, concerns have been expressed whether test conditions are really representative for reactor transients.
Typical values of pulse-width from RIA reactor calculations are in the range of 25ms to 75ms and even greater values for some BWR rod drop cases, whereas RIA experiments are performed with fast pulses in the range of 4-9ms. The consequences of this difference for the fuel rod failure mechanism should be determined.

The opinion was expressed that if burnup-dependent fuel failure limits were to be imposed, the analysis methodology used to demonstrate compliance to these limits should use realistic evaluation methods.

However, it is understood that assumptions on accident conditions and analysis methodology used in licensing evaluations will be determined by the licensing requirements.
A BEST-ESTIMATE ASSESSMENT OF ROD EJECTION FUEL DUTY IN PWRs

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Introduction

In August, 1994, the US Nuclear Regulatory Commission (NRC) issued Information Notice IN-94-64, notifying US utilities that recent experimental data (particularly from the CABRI test reactor) indicated that high burnup fuel undergoing a fast reactivity insertion accident (RIA) could undergo cladding failure at a much lower deposited energy in the fuel than was previously assumed. The NRC stated that higher burnup fuel may be required to meet more restrictive criteria than those currently allowed for fuel failure, and that the NRC review of licensee requests to extend fuel burnup limits beyond those currently licensed would carefully consider this data.

For PWRs, the NRC has identified the postulated control rod ejection accident as the RIA event of concern for this issue. It has become quite clear that in order to identify the impact of this new data, a very good best estimate knowledge of the enthalpy deposition during a rod ejection transient is needed. Therefore, Westinghouse is currently performing a study of the control rod ejection accident using detailed three-dimensional transient core neutronics calculations to determine the significance and limiting conditions for the event when compared to postulated fuel cladding failure limits, while recognizing that these limits have not yet been set by the NRC. The initial intent of these calculations is to determine the margins that are available from the use of three dimensional space time kinetics methods as compared to the conventional 1-D kinetics methodology currently used by Westinghouse. Subsequently, should revised fuel failure limits be established by the NRC, the same methods can be used to perform a best-estimate determination of the fuel failures for the radiological dose analysis.

For this paper, current licensed (1-D kinetics) methodology was first used for determining the limiting scenarios with respect to the high burnup fuel RIA issue for current core designs. This was done based on a consideration of the control rod patterns and insertion limits, and different fuel cycle designs, in Westinghouse 2-, 3- and 4-loop plants. An extreme case was then chosen to perform the 3-D benchmark analysis. The 3-D results are then compared to the licensing basis methodology results. The results are presented in terms of nuclear power versus time, heat flux versus time, and the number of fuel rods that exceed various postulated failure limits (in cal/gm) for high burnup fuel.

Initial Analysis

The analysis that was performed to determine the limiting plants and cycles for the high burnup RIA issue used existing Westinghouse licensed methodology. The main calculational parameter of interest was the peak fuel enthalpy that would be experienced by high burnup fuel as a result of a control rod ejection transient. The analysis was performed by first performing three-dimensional static nuclear calculations to determine the number of fuel assemblies which experience high post-ejection power peaking...
factors and high burnups, and then combining this with the results of a one-dimensional transient nuclear calculation which determined the peak fuel enthalpy as a function of power peaking factor.

The preconditions for the transient kinetics calculations are based on maximum ejected rod worths and hot channel factors as calculated by the ANC\(^{(1)}\) computer program. Other key nuclear parameters used in the analysis included reactivity feedback weighting factors, moderator and Doppler coefficients, delayed neutron fraction, and trip reactivity insertion. The TWINKLE\(^{(2)}\) computer program was used to determine the average core power generation as a function of time including the various total core feedback effects (i.e., Doppler reactivity and moderator density reactivity). The fuel enthalpy and temperature transient at the hot spot were then determined by multiplying the average core power generation by the hot channel factor, as obtained from ANC, and performing a fuel rod transient heat-transfer calculation using the FACTRAN\(^{(3)}\) computer program.

The goal of the static ANC calculations was to first determine if there is a correlation between post rod ejection heat flux hot channel factors, \(F_Q\), and enthalpy rise and the corresponding assembly burnups. Secondly, the assembly population distribution as a function of post rod ejection \(F_Q\) was determined for several severe rod ejection simulations.

There exists numerous factors which may influence the core power distribution and power peaking during a rod ejection event, including:

- Core size
- Burnable absorber type
- Type of loading pattern
- Initial radial power distribution
- Power level
- Control rod pattern
- Fuel type
- Control rod insertion limits
- Presence of axial blanket fuel
- Time in Life

Twenty different high discharge burnup reload and hypothetical designs, some with lead rod burnups exceeding currently licensed limits, were studied. Two, three and four loop core designs were analyzed, utilizing various Westinghouse control rod patterns. Low leakage patterns, some including axial blankets, were evaluated, and in one instance a high leakage core was investigated. In addition to the twelve-foot cores, a ten-foot core and a fourteen foot core were also analyzed.

Detailed full-core, three-dimensional ANC static modeling of rod ejections was performed for the selected cycle of each plant. The Hot Zero Power (HZP) ANC models were conservatively preconditioned with control rods at typical zero power insertion limits. All calculations were performed using the standard reload procedure, with established uncertainties applied to all analytical results.

The transient was analyzed by calculating the peak values of energy deposition (cal/gm) that could be reached during the transient in various fuel assemblies in the core. These calculations were performed using the current Westinghouse licensing basis TWINKLE/FACTRAN rod ejection methodology\(^{(4)}\). The analysis was performed for the end-of-cycle HZP case, since this case results in the most severe nuclear power transient as well as the highest fuel burnups. For a specific ejected rod worth, the core average nuclear power transient was calculated once, but the transient fuel temperature calculation was performed several times with different transient peaking factors to represent the various fuel assemblies.

Following generation of the rod ejection cases, the peak quarter-assembly \(F_Q\) was converted to a cal/gm value based on the results of the transient analysis. Population studies for peak quarter-assembly cal/gm versus maximum quarter-assembly pin burnup were then made, to ascertain the distribution of energy deposition with pin burnup. In all, several hundred rod ejection cases were analyzed.
Initial Analysis Results

The results of the initial analysis of the post rod ejection peaking factors as a function of pinwise burnup indicated no direct correlation existed. The results show that a control rod ejecting from a low burnup fuel assembly can drive surrounding higher burnup fuel assemblies to a high $F_Q$. Conversely, a control rod ejecting from a high burnup fuel assembly can also drive surrounding low burnup fuel assemblies to a high $F_Q$. The results also indicated that the rod ejection event results in a very localized increase in peaking factors. The impact of the rod ejection on neighboring assemblies is related to the reactivity worth of the ejected rod. The results demonstrate that although there is no clear correlation of $F_Q$ with burnup, only a small percentage of the high burnup fuel assemblies in the core will be driven to very high $F_Q$ values.

Figure 1 shows a scatter diagram for an extremely limiting case analyzed in the initial study. The calculation for this case was performed at the end of a long (18-month) burnup cycle for a 157 assembly three-loop Westinghouse plant, by ejecting a control rod on the periphery of the core. It was assumed that the reactor was just critical at hot zero power, with the control rods at the insertion limits.

For this case, key parameters such as ejected rod worth, peaking factor, delayed neutron fraction and Doppler defects were chosen very conservatively, creating an extremely limiting scenario. As expected, the answers were very limiting; for instance, the fuel enthalpy limit of 200 cal/gm used by Westinghouse was exceeded. However, the results showed that even with the significant conservatisms inherent in this analysis, only a small percentage of fuel assemblies could be driven to high enthalpy levels, regardless of burnup. This case was then used as the reference for determining the benefits that could be obtained from 3-D analysis.
Three Dimensional Transient Core Neutronics Study

A detailed three-dimensional core neutronics transient calculation was performed for the extremely limiting case presented in the previous section. The three-dimensional calculation was performed using the same conservative transient parameters utilized in the 3-D static / 1-D transient calculation described above, so that a direct comparison could be made to the previous calculation. The key nuclear parameters in the calculation were:

Ejected Rod Worth 967 pcm
(0.967 % ak)
Ejected Rod Peaking Factor (F_Q) 23.47
Delayed Neutron Fraction .0043
Doppler-only Power Defect 840 pcm

The Westinghouse-developed three-dimensional transient core neutronics computer code, SPNOVA, was used. SPNOVA(5) is an extremely fast coarse-mesh neutronic nodal code which is one to two orders of magnitude faster than conventional nodal methods. The code can be used in one, two, or three dimensions. The SPNOVA calculation was performed using one node per assembly with 20 axial nodes. The SPNOVA code has been licensed with the USNRC, and its applicability to rod ejection problems has previously been demonstrated. The core heat transfer model used in the analysis is taken from the NRC-licensed Westinghouse safety analysis code NOTRUMP(6). The fuel rod calculation was performed for both an average fuel rod and a hot rod in each of the 157 assemblies.

Results of the Three Dimensional transient Core Neutronics Study

The results of the three-dimensional transient analysis versus the one-dimensional kinetics analysis are shown in Figures 2-5. The three-dimensional case results in a less severe nuclear power and heat flux transient (Figures 2 and 3) than predicted by the one-dimensional calculation since the one-dimensional case underestimates the effect of both Doppler and moderator feedback.

![Fig 2: Relative Nuclear Power versus Time](image)

![Fig 3: Heat Flux versus Time](image)

The hot spot fuel enthalpy vs. time for the two cases is shown in Figure 4. The peak fuel enthalpy reached during the transient, as calculated by the 3-D model, is approximately one-half of that predicted by the one-dimensional calculation. This is due partly to the reduction in the nuclear power transient, but also to the reduction in the total peaking factor (F_Q) vs. time as determined by the three-dimensional calculation. The total peaking factor vs. time for the two calculational methods is shown in Figure 5. Since there is no way to estimate this effect in a 1-D calculation, a constant total peaking factor was assumed in that calculation.
Finally, the peak fuel enthalpy versus burnup is plotted for all assemblies in the core for the 3-D study (Figure 6). These results show that even using very conservative input assumptions, the three-dimensional methods result in a significant increase in margin compared to the conventional licensing-basis methods used in the initial study. Additional three-dimensional studies that are currently underway using best-estimate parameters for the ejected rod worth, peaking factor, delayed neutron fraction, and Doppler feedback are expected to show that even more margin is available.

Conclusions

The results of the analysis performed to date indicate that significant margin can be obtained from 3-D core neutronic transient methodologies for the rod ejection event. Peak values of enthalpy deposition are reduced approximately by a factor of two, with a corresponding reduction in the number of fuel assemblies that would exceed postulated enthalpy deposition failure limits for the RIA event.
References


The document reports a study of the rod ejection accident for a LPWR plant in the context of high burnup fuel management. The transient is analysed in two steps, first using a standard licensed conservative approach, and second time on the basis of a 3D more physical methodology.

This paper has been prepared for the OCDE "Specialist Meeting on Transient Behavior of High Burnup Fuel" to be held in Cadarache FRANCE, from 12 th to 14 th september 1995.
STUDY OF THE ROD EJECTION TRANSIENT ON A PWR RELATED TO HIGH BURN-UP FUEL RUPTURE RISK.

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<td>INDICE C</td>
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<td>INDICE D</td>
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<td>INDICE E</td>
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Note transmise à la Mission Contrôle Externe

Par : Secrétariat / Date d'envoi : /

DIFFUSION

- DIRECTION
- Départ. THEORIE
- Départ. SYSTEMES ET INSTALLATION
- Départ. MATERIELS
- Départ. SURETE (si la note est IPS)
- Chef de projet concerné
- Chef de division rédactrice
- Ingénieur rédacteur
- Division GT : documentation (2 ex.)
- Division AF : bureau du courrier

COMPLÉMENTAIRE

EPN/DEX/GCN : M. BARRAL

dont SEPTEN

MM. DELBECQ - BERTHET - HOUDAYER
MM. VITTON - AGNOUX - PERMEZEL
MM. BERNAUDAT - WAECKEL

SECRETARIAT PN (2 EX)

252
STUDY OF THE ROD EJECTION TRANSIENT ON A PWR RELATED TO HIGH BURN-UP FUEL RUPTURE RISK.

Summary of the report

PURPOSE

1. ACCIDENT DESCRIPTION

2. CORE INITIAL STATE CONDITIONS

3. STANDARD METHODOLOGY ANALYSIS AND RESULTS
   3.1. Basic investigation of the limiting plant
   3.2. Standard method analysis extended to specific high burned rod
   3.3. Core initial state and associated basic cases
   3.4. Results of transients evaluation

4. 3D METHODOLOGY ANALYSIS
   4.1. Core space and time discretisation
   4.2. Basic cases evaluation
   4.3. Results of 3D calculations

GLOSSARY

Page 3/18
STUDY OF THE ROD EJECTION TRANSIENT ON A PWR RELATED TO HIGH BURN-UP FUEL RUPTURE RISK.

PURPOSE

Long cycle fuel management is considered today by EDF, and also by other utilities in the international context, as an economical priority for civil nuclear industry. For going on this way, high burnup fuel licensing must be obtained from the national safety authorities. One of concerns to obtain this agreement is to justify that high burnup fuel behavior remains compatible with plant safety during fast reactivity initiated accidents (RIA).

Large experimental tests programs have been performed in the past (SPERT, TREAT, PBF), basically on fresh fuel rods, completed with some tests at low burnup. The aim was to precise the failure process related to fast and important energy deposition, and to define the associated limiting criterias. To complete the experimental results on high irradiated fuel, other tests programs have been decided and engaged, in Japan first and in France later (NSRR and CABRI programs).

French experimental program, started at the end of 1993, is not totally completed yet. Tests realisation is performed on the reactor CABRI of the CEA (Cadarache Nuclear Center).

The first test has been performed on a standard fuel rod to high burnup (Enrichment : 4.5 % WU5, Burnup : about 62 GWj/t). Unfortunately a low enthalpy failure was obtained during this first test. After a comprehension work on this unexpected result, information of the international nuclear community has been made by the CEA/IPSN concerning the experience and the associated results.

Following tests on fuel rods irradiated to 32 and 52 GWj/t appear more conclusive, since failure was not reached for peak transient enthalpies near to 200 cal/g and 115 cal/g respectively.

In relationship with the first CABRI tests, reactor transient studies have been called by the french authorities to justify that no risk is incurred on actual plant operating during hypothetical fast reactivity initiated accidents. Together with an explanation of the rod failure process and a transposition of the experimental results to the on reactor transient, results of thermal response of high irradiated fuel were expected.

This previous work was attended, before going on into the acceptance process of high burnup strategy engaged by EDF.

One reports here part of the study performed by EDF for this purpose. Evaluation of high burned fuel thermal response is performed on the basis of the 4th class rod ejection accident, considered as the most limiting RIA for safety design studies on a PWR.

The study is made on a two levels approach :
- First, using the standard conservative method retained in the Safety Report analysis, relaxed and extended to burned fuel,
- Second, a more physical and less penalizing evaluation based on a 3D modelisation.
1. ACCIDENT DESCRIPTION

The postulated initiation event is the rupture of the rod command mechanism pressure carter. Rod ejection is supposed to occur due to the pressure difference to the break. The time assumed for the rod withdrawal out of the core is 0.1 second. The immediate core reactivity increase due to the absorbant removal may be in the order of several hundred pcm. This positive reactivity effect generates a fast increase of the power level together with a high local peak flux. If the rod is located to a peripheral position the local power peak is accentuated by the dissymmetry.

One considers two different possible cases

- The core overpasses the prompt criticality:
  If the rod removal reactivity effect \( \rho_c \) is larger than the delayed neutrons fraction \( \beta \), the core comes prompt critical. The induced power prompt jump is limited by the Doppler defect induced by the fuel temperature increase. The transient is a high but stright power pulse. The amplitude and the width of the pulse depend on the reactivity overpassing \( (\rho_c - \beta) \), as the preponderant parameter.

- The core doesn't reach the prompt criticality:
  Even for a rod worth less than \( \beta \), the core power increase is due to the prompt neutrons response. Nevertheless in this case the prompt jump is limited by the "delayed neutrons level" increase. In a second time the Doppler feedback effect will make the power decrease.

The reactor trip occurs by intervention of the core Protection System (High neutron flux, or Positive flux rate). The scram efficiency to limit the power transient is more important in the non prompt critical case. Any way, the scram is needed to ensure the core subcriticality.

2. CORE INITIAL STATE CONDITIONS

French 3 loop plants operate in that Relaxed Mode A (6 plants) or Mode G (other plants). Basic investigation of past experience shows Relaxed Mode A operating is the most limitative at HZP and HFP conditions for this accident.

The Mode A control banks position for rods which may be present at power is shown Figure 1.
STUDY OF THE ROD EJECTION TRANSIENT ON A PWR RELATED TO HIGH BURN-UP FUEL RUPTURE RISK.

Fig 1: Control Banks Positioning

CORE CONTROL RODS POSITION

- D Bank rods
- C Bank rods

RODS INSERTION (LIMITS)
3. STANDARD METHODOLOGY ANALYSIS AND RESULTS

3.1. BASIC INVESTIGATION OF THE LIMITING PLANT

As a preliminary work, investigation on the 900 MWe plants actual and earlier experience was necessary to identify the most limiting plant and cycle (loading pattern), related to this potential accident. This was obtained using the whole available results of specific reload safety calculations data bank (accident nuclear parameters). Therefore, this identification has been restricted to the actual fuel management: quarter core refueling, 3.7 %W U5 feed assemblies enrichment.

As a second work, the transient has been evaluated at the end of this limiting cycle for different initial conditions covering the plant operating mode: full range of power level, worst initial states maximizing individual rod ejection worth and associated local peak power factors (on low and high burned fuel rod).

3.2. STANDARD METHOD ANALYSIS EXTENDED TO SPECIFIC HIGH BURNED ROD

The method is based on a 2D-1D stationnary neutronic evaluation of the nuclear parameters. The corresponding values are then used for the simulation of the average core response. This evaluation is performed using a 1D (or a 0D) kinetic model. A parallel calculation of local fuel thermal response is based on a power peaking factor variation input law.

Common core 2D and 1D models used for nuclear design and plant operating simulation are used to perform the nuclear-thermohydraulic data, the initial state of the plant and the nuclear kinetic parameters related to the accident. The transient is then evaluated by an appropriated neutronic and thermohydraulic coupled model (EDF Altair code). Uncertainties are applied to nuclear parameters (ejected rod weight, feedback effects, scram weight, power peaking factors, fusion and DNBR limits,...) for a conservative study of the transient.

Nuclear parameters depend on initial plant state. Individual rod ejection worth and peaking factors highest values are obtained at low power level, due to the large insertion and number of control rods in the core. Calculations show that prompt criticality may be overpassed in these conditions, but lightly (core reactivity remains close to the total delayed neutrons effective fraction). When increasing the initial power level, rod worth and peaking factors decrease (less number and insertion of control rods). The potential transient turns generally into a simple prompt jump of less severe magnitude.
The EDF Altair code is a 0D neutronic kinetic model coupled with a 1D thermohydraulic rod and channel model. Even if neutronics is treated by a 0D model, the code takes into account 2D and 1D neutronic effects evaluated externally: rod and emergency scram axial differential worthes, radial and axial fuel effective temperature changes.

One does not take these considerations as general rules, nevertheless they apply on the present study.

### 3.3. Core Initial State and Associated Basic Cases

Evaluation on the limiting plant and results are reported for the two following specific cases:

**HZP Case**: Ejection of the peripheral D bank rod B08 at EOL - 0 % NP
- Penalizing axial core state obtained by xenon oscillation at intermediate power level
- Banks initial positioning to the insertion limits
- Reduced primary flow: 2/3 loops operating

**HFP Case**: Ejection of the peripheral D bank rod B08 at EOL -100 % NP
- Penalizing axial core state obtained by xenon oscillation at intermediate power level
- Banks initial positioning to the insertion limits
- Reduced primary flow: 3/3 loops operating

### 3.4. Results of Transients Evaluation

Detailed results for the two cases are reported:

- Figures 2 and 3: Nuclear power transient
- Figures 4 and 5: Local thermal response at high burnup

Reactor power transient characteristics depend on the operating plant mode and its initial state:
- prompt critical pulse: high magnitude, but short duration (many times the nominal power, mid-height time interval in the order of 0.1 sec),
- simple prompt jump: step of less magnitude (less than one time the nominal power), but more large duration (0.5 to 1 s).
STUDY OF THE ROD EJECTION TRANSIENT ON A PWR RELATED TO HIGH BURN-UP FUEL RUPTURE RISK.

Energy deposition in the pellet is obtained due the transient shortness, which causes a mismatch between energy production and transfer from pellet to coolant. Maximum energy deposition may be obtained in the case of a prompt critical pulse at low initial power. For the simple prompt jump case, energy deposition is lower, but the transient enthalpy peak may be of same order, if the initial power is substantial (since initial fuel enthalpy is higher).

DNBR may be obtained for low initial power transient when the core comes prompt critical. This result is obtained on the present treated case, taking into account uncertainties on the critical flux limit. This is not the case for the other initial power conditions (more than 10 % NP).

Summary of results obtained with the standard licensed methodology is reported below.

Nuclear parameters and transient response:

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Low initial power</th>
<th>High initial power</th>
</tr>
</thead>
<tbody>
<tr>
<td>Initial power</td>
<td>~ 0.01 NP</td>
<td>1.00 NP</td>
</tr>
<tr>
<td>Bank rod reactivity</td>
<td>~ 550 pcm</td>
<td>~ 90 pcm</td>
</tr>
<tr>
<td>Effective fraction of delayed neutrons</td>
<td>460 pcm</td>
<td>460 pcm</td>
</tr>
<tr>
<td>Peak value of average nuclear power</td>
<td>~ 4.8 NP</td>
<td>~ 1.24 NP</td>
</tr>
<tr>
<td>Peak value of burned fuel core relative power</td>
<td>-2.87</td>
<td>~ 1.21</td>
</tr>
<tr>
<td>Initial peak value</td>
<td>~ 10.5</td>
<td>~ 3.5</td>
</tr>
<tr>
<td>Final peak value</td>
<td>~ 0.08 s</td>
<td>~ 0.5 s</td>
</tr>
<tr>
<td>Mid-height pulse time width</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Transient type</td>
<td>Prompt critical pulse</td>
<td>Simple prompt jump</td>
</tr>
</tbody>
</table>

Thermal response of burned fuel (local fuel rod burnup : 50 GWd/t)

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Energy deposition</td>
<td>20 to 40 cal/g</td>
</tr>
<tr>
<td>Peak enthalpy</td>
<td>60 cal/g</td>
</tr>
<tr>
<td>Pellet peripheral peak temperature</td>
<td>720 °C</td>
</tr>
<tr>
<td>(Slight Rim)</td>
<td></td>
</tr>
</tbody>
</table>
STUDY OF THE ROD EJECTION TRANSIENT ON A PWR RELATED TO HIGH BURN-UP FUEL RUPTURE RISK.

Fig 2: P=0 % NP - Mode A - Gravelines 3 cycle 8 Plant
Ejection of BD8 Rod (D Bank) - High Flux Tip: 35 % NP

Fig 3: P=100 % NP - Mode A - Gravelines 3 cycle 8 Plant
Ejection of BD8 Rod (D bank) - High Flux Tip: 118 % NP
STUDY OF THE ROD EJECTION TRANSIENT ON A PWR RELATED TO HIGH BURN-UP FUEL RUPTURE RISK.

Fig. 4: HnP Case - Thermal response of high burnup fuel (50 GWD/tn)

Fig. 5: HnP Case - Thermal response of high burnup fuel (50 GWD/tn)
4. 3D METHODOLOGY ANALYSIS

In the aim to get a more realistic appreciation of the transient, a 3D evaluation has been performed by EDF.

The 3D COCCINELLE code has been used for this study. This code is able to deal the coupled process of kinetic neutronics and rod-channel thermohydraulics. A core fine coarse mesh geometry has been defined in the scope to access directly to power and thermohydraulic local response. This approach avoids penalties which may be induced by separate treatment of physical effects. In the coupled process, no particular physical phenomena has been identified to be treated artificially in a non conservative way, due to simplifications.

The two previous cases have been reevaluated by 3D calculations.

4.1. CORE SPACE AND TIME DISCRETISATION

Spatial description:
A fine coarse mesh has been used to modelise the core:
- radially (x,y) : 4 by 4 meshes per assembly on active section
- axially (z) : 90 intervals on active length
- radial and axial reflectors are treated explicitly with an extension of the spatial meshing.

The same fine meshing is used for neutronics, fuel rod thermal response and channel thermohydraulics coupled calculation. The spatial description is shown Figure 6.

Time discretisation:
The time interval is imposed by the neutronic kinetics. For this study the same value is used to treat in a coherent way neutronics and thermal heat transfer.

An optimisation of the time step has been performed supported by analytic considerations with the purpose to minimise the numerical error due to the discretisation process and its diffusion with time.
4.2. BASIC CASES EVALUATION

The two previous cases analysed with the standard conservative approach have been reevaluated using the 3D modelisation. The same initial state (power, flow, xenon, and banks position) is considered but its representation is performed by 3D stationnary calculations.

The same uncertainties are considered on nuclear and kinetic parameters. Nevertheless no penalty is considered on the thermal critical flux related to the DNBR status. After calculation check is made to appreciate the realistic margin to DNBR.

The transient evaluation is accomplished using the following discretisation hypothesis for neutronics and thermohydraulics:

- HZP Case :  - Flux precision : 5 E-5 %
  - Time step 2 E-3 sec (power pulse phase)
- HFP Case :  - Flux precision : 5 E-5 %
  - Time step 5 E-3 sec (power jump phase)

The emergency scram is simulated with a delay of 0.6 sec after trip setpoint validation.

4.3. RESULTS OF 3D CALCULATIONS

Results of the evaluation are reported Figures 7 to 10.

Summary of results given by the 3D methodology calculations is reported below.

Nuclear parameters and transient response:

<table>
<thead>
<tr>
<th>Parameter</th>
<th>[Low initial power]</th>
<th>[High initial power]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Initial power</td>
<td>~ 0.01 NP</td>
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<td>Bank rod reactivity</td>
<td>&gt; $\beta$</td>
<td>&lt; $\beta$</td>
</tr>
<tr>
<td>Effective fraction of delayed neutrons</td>
<td>460 pcm</td>
<td>460 pcm</td>
</tr>
<tr>
<td>Peak value of average nuclear power</td>
<td>~ 4.8 NP</td>
<td>~ 1.10 NP</td>
</tr>
<tr>
<td>Peak value of burned fuel core relative power</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Initial peak value</td>
<td>~ 2.41</td>
<td>~ 1.07</td>
</tr>
<tr>
<td>Final peak value</td>
<td>~ 8.55</td>
<td>~ 1.406</td>
</tr>
<tr>
<td>Transient type</td>
<td>Prompt critical pulse Simple prompt jump</td>
<td></td>
</tr>
</tbody>
</table>
STUDY OF THE ROD EJECTION TRANSIENT ON A PWR RELATED TO HIGH BURN-UP FUEL RUPTURE RISK.

Thermal response of burned fuel (local fuel rod burnup : 50 GWd/t)

- Energy deposition : 15 cal/g (depending of pulse type)
- Peak enthalpy : 50 cal/g (depending of pulse type)
- Pellet peripheral peak temperature (Slight Rim) : 500 °C (prompt critical pulse)

Average nuclear response given by the 3D method appears to be close to the one obtained with the standard approach, when penalties on the nuclear parameters are taken into account (rod worth, feedbacks effects, scram worth,...).

Nevertheless, the coupled dynamic evaluation of core power generation and transfer brings less penalizing results in terms of core power distribution and associated local fuel rod thermal response. As a summary, peak energy deposition is reduced to closely 15 cal/g and absolute peak enthalpy doesn't overpass 50 cal/g. This favorable results are however partly due to the fact that no consideration of uncertainties is taken into account related to thermal critical flux limit, which is not not reached in these conditions.

On concern of fine spatial coarse meshing and timing resolution, this study is certainly a first experience. More practice is however necessary in the aim to get a licensing. On calculation cost point of vue, the 3D method is expensive in terms of computer capacity and time consuming. This is due to fine coarse mesh, and specially to feedbacks iterative treatment.

Today one can consider the 3D approach not adapted for industrial repetitive applications, but its interest as a contribution to fuel licensing may be retained.
STUDY OF THE ROD EJECTION TRANSIENT ON A PWR RELATED TO HIGH BURN-UP FUEL RUPTURE RISK.

Fig 6: 3D spatial core discretisation

AXIAL REFLECTOR: 3 MESH
ACTIVE HEIGHT: 88 MESH

RADIAL ASSEMBLY
MESH: 4 x 4
RADIAL REFLECTOR
SAME MESHING

○ BANK D
□ BANK C
STUDY OF THE ROD EJECTION TRANSIENT ON A PWR RELATED TO HIGH BURN-UP FUEL RUPTURE RISK.

Fig 7: P=0 % NP - Mode A - Gravelines3 cycle 8 Plant
Ejection of BOS Rod (D Bank) - 3D Calculation

Fig 8: P=100 % NP - Mode A - Gravelines3 cycle 8 Plant
Ejection of BOS Rod (D Bank) - 3D Calculation
STUDY OF THE ROD EJECTION TRANSIENT ON A PWR RELATED TO HIGH BURN-UP FUEL RUPTURE RISK.

Figure 9: HgP Case - Thermal response of high burnup fuel (60 GWd/t)
3D Calculation

- $T_{th}$
- $T_{max}$
- $T_{RM}$
- $T_{if}$

Time (sec) 0 0.5 1 1.5 2 2.5 3

Temperature (°C) 0 100 200 300 400 500 600

Figure 10: HP Case - Thermal response of high burnup fuel (80 GWd/t)
3D Calculation

- $T_{th}$
- $T_{max}$
- $T_{RM}$
- $T_{if}$

Time (sec) 0 0.5 1 1.5 2 2.5 3 3.5 4

Temperature (°C) 0 200 400 600 800 1000
STUDY OF THE ROD EJECTION TRANSIENT ON A PWR RELATED TO HIGH BURN-UP FUEL RUPTURE RISK.

GLOSSARY

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\rho_e$</td>
<td>Reactivity of the ejected rod</td>
</tr>
<tr>
<td>$\beta$</td>
<td>Delayed neutrons fraction</td>
</tr>
<tr>
<td>HZP</td>
<td>Hot Zero Power</td>
</tr>
<tr>
<td>HFP</td>
<td>Hot Full Power</td>
</tr>
<tr>
<td>EOL</td>
<td>End Of Life</td>
</tr>
<tr>
<td>NP</td>
<td>Nominal Power</td>
</tr>
<tr>
<td>FN</td>
<td>Nuclear average core power</td>
</tr>
<tr>
<td>FTH</td>
<td>Thermal average core power</td>
</tr>
<tr>
<td>Taf</td>
<td>Average fuel temperature</td>
</tr>
<tr>
<td>Tsf</td>
<td>Fuel surface temperature</td>
</tr>
<tr>
<td>Trim</td>
<td>Fuel external rim temperature</td>
</tr>
<tr>
<td>Tig</td>
<td>Clad internal temperature</td>
</tr>
<tr>
<td>DNBR</td>
<td>Departure of Nucleate Boiling Ratio</td>
</tr>
</tbody>
</table>
ON THE ROLE OF BURNUP EFFECTS OF FUEL PROPERTIES IN RIA ANALYSES

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INTRODUCTION

In VTT Energy a reactor dynamics calculation system has been created for independent transient and accident analyses in Finland. It includes the three-dimensional hexagonal dynamics code HEXTRAN for VVERs [1, 2] and the axially one-dimensional dynamics code TRAB for BWRs [3, 4]. In both dynamics codes the fuel, gas gap and cladding properties are functions of the local or pellet average fuel temperatures. Different types of fuel with different properties can be included into the models of the reactor core and in HEXTRAN even the actual burnup in each axial node of fuel assemblies can be taken into account. Usually only the gas gap properties have been varied in the analyses due to the large uncertainty associated with their values. Now, new data is available [5] and it has been utilized to carry out more detailed calculations also with different fuel pellet conductivity values depending on burnup. Reactivity initiated accident (RIA) analyses of control rod group withdrawal and control rod ejection accident have been analyzed with the three-dimensional HEXTRAN code.

VTT'S CALCULATION SYSTEM FOR REACTOR DYNAMICS

VTT's calculation system for reactor dynamics is in Fig. 1. It consists of codes for both types of Finnish plants: BWR and VVER reactors. The figure clearly reveals that many well-known and widely-used codes have been acquired through data banks (e.g., OECD/NEA, RSIC) to complete VTT's code system. As compensation, some Finnish codes have been delivered to the data banks. In Finland the nuclear data is calculated with CASMO [6] and CASMO-HEX [7] on the basis of different evaluated data libraries (e.g., ENDF/B, JEF-2 or JENDL-3). CASMO-HEX is a hexagonal adaptation of the Swedish CASMO code. The Finnish CASMO-HEX / HEXBU-3D VVER code system [8,9] has been validated against plant measurements and in the TIC co-operation utilizing the results of its comprehensive experimental program and comparative calculations between different codes [10]. The properties of fuel in different conditions are predicted at VTT by the British ENIGMA code [11, 12, 13].

The system is widely utilized in contract research for the nuclear safety authorities and power companies. The four Finnish reactors, some foreign plants and potential new plant concepts have been studied. The possible causes of the Chernobyl reactivity accident were evaluated during 1986 - 1988 [14, 15]. Recently, further development of the code system also for RBMK type reactors has started again.
Basic nuclear data

Nuclear data processing

Nuclear data libraries (25 - 70 energy groups)

Calculation of assemblywise two-group constants

Calculation of reactivities, power and burnup distributions etc.

Data transfer and condensation for one-dimensional group constants

One-dimensional dynamics codes

Three-dimensional dynamics codes

\(\square\) = codes developed by VTT
\(\square\square\) = codes partly developed by VTT
\(\square\square\square\) = codes applied by VTT

Figure 1. VTT Energy's calculation system for reactor dynamics.
All reactor dynamics codes in Fig. 1 have been developed at VTT. The main new development targets at present are: modelling of the neutron kinetics three-dimensionally also in rectangular geometry and renewing of the thermal hydraulic solution methods.

The three-dimensional code HEXTRAN accurately describes the VVER core consisting of hexagonal fuel elements. The neutron kinetics model of HEXTRAN solves the two-group diffusion equations in homogenized fuel assembly geometry by a sophisticated nodal method. Within nodes the time-dependent two-group fluxes are represented by linear combinations of two time-dependent spatial modes, the fundamental and the transient mode of solution. The heat transfer calculation with several radial mesh points is made for an average fuel rod in each fuel assembly divided axially in several regions. The release of prompt and delayed nuclear heat in fuel or in coolant is modelled. The heat conduction equation is solved according to Fourier's law using different heat transfer coefficients for different hydraulic regimes. Fully realistic accident analyses starting from actual fuel cycle conditions can be made. HEXTRAN has been dynamically coupled with the thermal hydraulic circuit model SMABRE [16] for the primary and secondary loop calculations. Main applications of HEXTRAN are analyses of asymmetric accidents in the reactor core originating from neutronic or thermal hydraulic disturbances in the core or the cooling circuits, such as control rod ejection, main steam line break, local boron dilution or startup of an inoperable loop.

There is not very much experience in the world in carrying out conservative accident analyses with a best-estimate three-dimensional reactor dynamics code. Possibilities to modify the neutronics parameters have been added to HEXTRAN so that the conservativity of the calculations can be simply and reliably modified without changing the vast ordinary neutronics data. Also a new multiple hot channel methodology has been developed for this purpose.

The TRAB code includes a one-dimensional description of the core geometry, neutronics, rod heat transfer, and thermal hydraulics, using at most three parallel axial channels. In neutron kinetics a synthesis model composed of a time-dependent axial two-group diffusion equation and a radial shape function equation can be employed. TRAB models also the main BWR circulation system inside the reactor vessel, including the steam dome with related systems, steam lines, recirculation pumps, incoming and outgoing flows as well as control and protection systems. In the power excursion transients, the extreme phenomena modelled are the fuel temperature rise after occurrence of the boiling crisis and oxidation of the cladding material. The core model of TRAB is used separately for hot channel analyses on the basis of the output files of the main calculations made with HEXTRAN or TRAB. Thus sensitivity studies can easily be made on the basis of the power history of several fuel assemblies using, e.g., different fuel properties.

On the basis of the experience of the HEXTRAN development, also the TRAB core neutronics model is now being converted to three dimensions. The nodal solution method is modified to rectangular geometry for BWR and RBMK calculations. The new code will be called TRAB-3D.

Both HEXTRAN and TRAB have been validated with benchmark problems, comparative calculations, and simulation against both experiments and real plant transients. The HEXTRAN code has been extensively applied for the accident analyses of the Finnish VVER-440 type Loviisa power station [17, 18], for the new Russian concept VVER-91 [19], and also for the Hungarian VVER-440 type Paks NPP [20]. The BWR calculation system has been utilized for comparative and licensing transient analyses of the Finnish ABB Atom type BWR Olkiluoto.
Fig. 2 Stationary relative power distribution and the average burnup distribution (Gwd/tnU).

Maximum fuel center temperatures (C) at maximum power during CR withdrawal

Fig. 3 Maximum fuel center temperatures of fuel assemblies calculated with two types of data: with burnup dependent fuel conductivity or with fresh fuel data; at the time of the maximum power during control rod withdrawal transient.
In LWR accident analyses, many complicated phenomena arise which cannot be correctly solved by the hydraulic solution methods applied so far in the world. Presently the thermal hydraulics models of the reactor dynamics codes of VTT are being further developed based on the new solution method PLIM: Piecewise Linear Interpolation Method [21]. The superiority of the PLIM algorithm in calculating propagation of a boron front through the reactor core in natural circulation conditions has been demonstrated with HEXTRAN calculations [22].

The new physically based two-phase flow model SFAV, Separation of two-phase Flow According to Velocity, have been successfully tested against measurements and analytical solution [23, 24]. In the future SFAV model will be applied in the reactor dynamics codes of VTT. They have now four equations in their hydraulics model. Especially in the calculation of film boiling in hot channel analyses, the six equation model would be preferable [25].

RIA ANALYSES WITH BURNUP DEPENDENT FUEL CONDUCTIVITY MODEL

Slow and fast RIA analyses of control rod group withdrawal and control rod ejection accident, respectively, were used as examples analyzing the burnup effects of fuel properties. They were carried out for a VVER-440 type reactor in typical EOC conditions with the three-dimensional HEXTRAN code. The consequences of the degradation of the fuel pellet conductivity with increasing burnup were studied. Comparison calculations were made using two different input data for the conductivity: curves for fresh fuel without any burnup dependence and curves with terms depending on the actual burnup of each calculational node. The data of the UO$_2$ conductivity degradation given in Ref. [5] was used: e.g., the fuel conductivity at 727 °C deteriorates over 20 % from its original value with modest assembly average burnups of 40 MWd/kgU.

The control rod group withdrawal transient calculation was carried out initiating from nominal power level. Fig. 2 shows the stationary relative power distribution and the average burnup distribution (Gwd/tnU) of the fuel assemblies in 1/6-symmetry of the core. The regulating control rod group is situated in locations 1 and 7, (location numbers are in the figures at the upper edge of the hexagons). There are fuel assemblies with high burnup values also in the middle part of the core.

In Fig. 3 the temperatures are shown at the time of the maximum power during control rod withdrawal transient just before the trip actuation from 112 % power level. There are the maximum fuel center temperatures of assemblies calculated with two types of data: with burnup dependent fuel conductivity or with fresh fuel data. The highest temperature is in both cases at location 14 where also the power level is highest and the burnup is low: 999 °C and 928 °C, relative power 1.26 in stationary state. However, the maximum temperature of the high burnup assembly at location 6, near the moving control rod in location 7, is also quite high, 933 °C, in the case with degraded fuel conductivity, although its power level is only 1.00 in the stationary state. The difference between the maximum temperatures of these assemblies decreases from 138 °C to only 66 °C when the effect of the fuel conductivity degradation is taken into account.

Similar effects in the maximum temperatures of assemblies with different power and burnup levels could be seen already in the stationary state at nominal power level.
Power distribution at time of maximum fission power during a CR ejection in VVER-440 from low power.

Relative power
Averange burnup
SE = SHIELD ELEMENT

Fig. 4 Relative power distribution at the time of the maximum power peak in the control rod ejection case from low initial power, and the average burnup distribution (Gwd/tnU).

Fig. 5 Relative power distribution at the time of the maximum power peak in the control rod ejection case from low initial power level.
The control rod ejection accident calculations were also made in EOC conditions, the burnup distribution was the same, see Fig. 4. Half core symmetry was used in the calculations, the control rod was ejected from the location 7. The resulting maximum power peaks were 2300 MW and 49 000 MW from nominal (1375 MW) and low (1 %) power levels, respectively. Fig. 4 shows the relative power distribution at the time of the maximum power peak in the case from low initial power level, graphically it can be seen in Fig. 5. Fig. 6 shows the maximum temperatures of assemblies in the case from low initial power level at time 1 s. The use of the burnup-dependent fuel conductivity model does not markedly change the temperatures because the initial enthalpy of the fuel is low and the transient is very fast and almost adiabatic.

Fig. 7 shows the maximum temperatures of assemblies in the control rod ejection accident case from nominal power level. The ejection distorts the power distribution more weakly but qualitatively in the same way as in the case from low initial power level, see Fig. 5. The power increase is largest at the outer edge of the core. In this example core, no high burnup fuel is loaded in the most critical part of the core during this accident: between the ejected rod in location 7 and the edge of the core. Therefore the highest temperature occurs in both cases again in location 14 with fresh fuel and high initial power level. However, the temperature distribution near the ejected control rod is very even in the calculation case with the burnup-dependent fuel conductivity model included, the number of fuel assemblies with high temperatures is much larger than in the calculational case with using fresh fuel conductivities.

CONCLUSIONS

Reactivity initiated accident (RIA) analyses of control rod group withdrawal and control rod ejection accident were analyzed. The consequences of the degradation of the fuel pellet conductivity with increasing burnup were studied. The results showed that there was considerable increase in fuel temperatures of assemblies with higher burnup when the fuel conductivity degradation was taken into account. The analyses were carried out with the three-dimensional reactor dynamics code HEXTRAN so it can also be concluded that the Doppler feedback effect cannot fully compensate for the temperature increases by decreasing the fission power levels of hot assemblies. The fuel temperature increase was seen in slow and fast RIAs and in the stationary states at nominal power level. The effect is stronger in slow transients initiating from nominal power level.

The conclusion is that it cannot be assumed in the RIA calculations that all the highest temperatures or enthalpies always occur in the fresh fuel assemblies, i.e., in the assemblies with highest power density. High temperatures can also be found in assemblies with high burnup if they are loaded in the middle part of the reactor core or near the control rods. The effect must be considered when the maximum enthalpy criteria or core loading types are planned.

REFERENCES


Maximum fuel center temperatures (°C) after CR ejection from low power

1. Burnup dep. fuel cond.
2. No burnup dep. fuel cond.
SE = SHIELD ELEMENT

Fig. 6 Maximum fuel center temperatures of fuel assemblies calculated with two types of data: with burnup dependent fuel conductivity or with fresh fuel data; the control rod ejection case from low initial power level.

Maximum fuel center temperatures (°C) after CR ejection from nominal power

1. Burnup dep. fuel cond.
2. No burnup dep. fuel cond.
SE = SHIELD ELEMENT

Fig. 7 Maximum fuel center temperatures of fuel assemblies calculated with two types of data: with burnup dependent fuel conductivity or with fresh fuel data; the control rod ejection case from nominal initial power level.


ANALYSIS OF THE FUEL BEHAVIOR UNDER ROD EJECTION ACCIDENT IN THE PRESSURIZED WATER REACTOR

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ABSTRACT

Fuel behavior under the rod ejection accident in a typical Westinghouse-designed 1,000 MWe PWR with the 17 x 17 fuel assembly has been analyzed using the three-dimensional nodal transient neutronics code, PANBOX2, to predict the local fuel rod power as well as total core power, and using the transient fuel performance analysis code, FRAP-T6 to predict the transient fuel rod behavior, such as fuel enthalpy, temperature and cladding strain. Fuel failure criteria versus the burnup was conservatively derived considering the currently available test data and the possible fuel failure mechanism. Analysis of the various fuel loading patterns in the core showed that fresh or once burned fuel assemblies would be more likely located in the D-bank position where a control rod is assumed to be ejected, and twice or thrice burned fuel assemblies could be located next to the D-bank position. Since transient power level strongly depends on the ejected rod worth, conservatism has been given by decreasing 10 percent for the delayed neutron fraction and increasing 20 percent for the absorption capability of the ejected control rod. Two cases of the high burnup and longer cycle length fuel loading schemes of a peak rod burnup of 54 MWD/kgU and 68 MWD/kgU, respectively, were selected for the analysis. The analysis used the same core conditions and assumptions as the conventional zero dimensional analysis. Three-dimensional analysis gave the peak fuel enthalpy during the rod ejection accident less than about one third of that calculated by the conventional zero dimensional analysis methodology. It also made it possible to predict the power variation of all the fuel rods in the core, and therefore, the fraction of fuel failure was estimated considering the distribution of the fuel enthalpy and burnup in the core. The results showed that the current design limit of less than 10 percent fuel failure and maintaining the core coolable geometry would be adequately satisfied under the rod ejection accident, even assuming the conservative fuel failure criteria, derived from the currently available test data.

1. INTRODUCTION

Performance of the high burnup fuel under the rod ejection accident in the PWR has recently been of great concern as several recent results of the simulated RIA tests in the research reactors shown in Fig. 1 indicated the need to revise the current fuel failure and the core damage criteria which had been determined based upon the test results of the unirradiated fuel rods. In fact, the fuel industry once revised the fuel failure and the core damage criteria after the burnup dependency of the criteria was alerted in the 1980s[1]. However, recent CABRI test results[2] raised the concern that the failure threshold of the high burnup fuel may be significantly decreased due to the degradation of the fuel properties at high burnup.

It can be said that the current analysis methodology of the rod ejection accident in the PWR employs a significant conservatism since the uncertainties in neutronics and transient fuel behavior prediction were compensated by allowing
the conservatism as long as the consequence of the accident is within the allowable safety limit. However, as there emerges an indication that the current design criteria on the fuel behavior under the rod ejection accident need to be revised, a need to re-analyze the current analysis methodology and remove reasonably the over-conservatism also arises. The major area for that is the transient neutronics prediction where zero dimensional analysis is employed. The improvement of the neutronics code and computing power made it possible to simulate the entire transient core three dimensionally. This realistic prediction could significantly decrease the over-conservatism in the prediction of transient core power.

In this study, the three dimensional core behavior under the rod ejection accident with same conservative assumptions used in the conventional zero dimensional analysis will be performed and the fuel failure criteria based upon the currently available test results will be newly derived and applied to check whether the radiological consequence analysis in the current safety analysis report with the assumption of 10% fuel failure is still applicable or re-analysis of the radiological consequence is necessary.

2. FUEL BEHAVIOR ANALYSIS UNDER ROD EJECTION ACCIDENT

2.1 Three Dimensional Analysis Methodology

Rod ejection accident analysis can be divided largely into three steps such as transient power calculation, fuel behavior analysis and radiological consequence analysis. Among them, the first two steps will be covered in this work.

The current fuel design criteria under the rod ejection accident in the pressurized water reactor is that the maximum radially averaged fuel pellet enthalpy for the irradiated fuel is less than 200 cal/gm to prevent the core damage and the fuel would fail if DNB occurs during the transient. The safety analysis results by the conventional analysis methodology showed that the fractional fuel failure was less than 10% and the core damage was prevented. Results of the radiological consequence analysis in the safety analysis report with the assumption of 10 % fuel failure were well within the safety limit.

Fig. 2 shows the analysis flow diagram of the rod ejection accident analysis in this work. At first, the relevant core loading pattern is selected for the analysis and neutronics calculation of the transient was then performed by the PANBOX2 code[3] to predict the core and the fuel rod power change during the transient. The PANBOX2 code was developed by Siemens and validated through the comparison with the several benchmark results calculated by the transient neutronics code such as PANTHER [4]. Then, the fuel rod behavior during the transient was predicted with the rod power histories from PANBOX2 code by FRAP-T6[5] which was developed in Idaho National Engineering Laboratory and validated by comparing with the in-reactor test results including the RIA test results.

Transient fuel behavior is then interpreted in terms of such fuel performance parameters as fuel temperature and enthalpy, and cladding stress and strain which FRAP-T6 can predict. Even though the transient power histories of all the fuel rods in the core can be provided by the PANBOX2 code, it was decided practically that the behavior of the selected fuel rods were predicted by the FRAP-T6 and for all the rods the transient power histories were simply integrated to estimate the fuel enthalpy increase during the transient by assuming that the heat generated in the fuel during the transient was not released out of the fuel at all since the transient usually ends in less than 0.4 sec for the HZP case and it ends in less than 2.0 sec for the HFP case. Figs. 3 and 4 compares the enthalpy increase calculated by the power integration method with the results of FRAP-T6 prediction for the HZP and the HFP cases. It can be seen that the differences are about 15 to 22% for the HZP case and about 40 to 60% for the HFP case. The difference depends directly upon both the duration of the transient and the gap conductance or the gap width during the transient which controls the heat transfer out of the fuel pellet. Anyhow, the power integration method to calculate the fuel enthalpy increase during the
transient over-estimates at least 15% and therefore is conservative.

Finally, the amount of the fuel failure will be estimated by comparing the calculated results of the enthalpy increase with the newly derived fuel failure criteria. The fuel failure criteria will be revised considering the published test results and the possible fuel failure mechanism.

2.2 Transient Power Calculation

To predict the core and the fuel rod power distributions during the rod ejection transient, PANBOX2 code was used with a node per fuel assembly in the half core simulation. PANBOX2 code is a nodal transient neutronics code coupled with a thermal-hydraulic dynamics code, COBRA and solves the space-time dependent neutron diffusion equation using nodal expansion method (NEM) and/or nodal integration method (NIM). To conservatively evaluate the transient power variation the core conditions and assumptions in this analysis are kept identical to those in the conventional design methodology, as shown in Table 1. The only difference is that three dimensional analysis is used instead of conventional zero dimensional analysis. Transient power level depends strongly on the ejected rod worth which is a function of the absorption capability of the ejected control rod and the delayed neutron fraction. Therefore, conservatism is given by decreasing 10 percent for the delayed neutron fraction and increasing 20 percent for the absorption capability of the ejected control rod.

Analysis of the various fuel loading patterns in the Westinghouse-design 1,000 MWe core showed that fresh or once burned fuel assemblies are more likely located in the D-bank position where a control rod is conservatively assumed to be ejected, and twice or thrice burned fuel assemblies can be located next to the D-bank position. Then, two cases of the fuel loading schemes were selected for the analysis. One is the low leakage and 18 month cycle length scheme with a peak rod burnup of 54 MWD/kgU and the other is the scoping ultra low leakage and 18 month cycle length scheme with a peak rod burnup of 68 MWD/kgU.

The transient Pin Maximum Linear Heat Generation Rate (PMLHGR) of all the fuel rods in the core after rod ejection is determined from the transient core power level and total pin peaking factor, $F_a$ values as follows:

$$PMLHGR(t) = ALHGR*P(t)*F_a$$

where,

$ALHGR$ = average linear heat generation rate (w/cm)

$F_a$ = total pin peaking factor ($F_{xyz}$) including uncertainties and technical tolerances

$P(t)$ = instant core power level

2.3 Fuel Rod Behavior During the Transient

Fuel rod behavior under the rod ejection accident was predicted by the fuel transient code FRAP-T6 using the rod power histories generated by PANBOX2 code. The performance parameters of concern were fuel centerline temperature, pellet enthalpy and the cladding strain. Even though FRAP-T6 does not have the capability to model the property degradation of the high burnup fuel such as pellet thermal conductivity, the radial power peaking in the pellet outer rim region at high burnup can be inputted into the FRAP-T6 code.

Figs. 5 and 6 illustrate the transient fuel rod power, fuel enthalpy, fuel centerline temperature and the cladding strain predicted by the FRAP-T6 code during the rod ejection transients for the HZP and the HFP cases, respectively. They show that the width (in FWHM) of the transient power pulse is about 35 msec for the HZP case and about 0.8 sec, and fuel enthalpy and cladding strain increase in a very short time.

The fuel failure criteria was newly derived with the simulated RIA test results in the research reactor and FRAP-T6 prediction, considering that fuel failure in the CABRI test-Na.l[2] for the fuel rod of 63 MWD/kgU burnup might occurred at the
total enthalpy of 30 cal/gm or enthalpy increase of 15 cal/gm, possibly by the mechanism of PCMI(Pellet Cladding Mechanical Interaction).

Fig. 7 shows the initial fuel enthalpy as a function of the fuel rod power level before the transient, where the scattering in the data point is caused by the fuel gap conductance changing with the burnup. For the HFP case, for example, the fuel rods with the power level over about 126 w/cm before the transient have the fuel enthalpy already higher than 30 cal/gm so that it is difficult to apply this low fuel failure enthalpy limit for the HFP case. And, if it is accepted that the fuel failure at the CABRI test-Na.1 occurred by the PCMI, it would be better to set the fuel failure criteria via the cladding strain or the fuel enthalpy increase during the transient rather than the fuel enthalpy.

Fig. 8 shows the cladding strain versus the enthalpy increase for the HZP and the HFP cases. The cladding strain depends upon not only the enthalpy increase but also the initial gap width which determines the time of gap contact and the cladding strain after gap contact. If the fuel rod of the CABRI test-Na.1 which is similar to the HZP case was gap-contacted at the end of the pre-irradiation, then the cladding strain of the fuel rod at the time of failure directly depends upon the final power level during the pre-irradiation as shown in Fig. 8. If the cladding failure is assumed to occur at the strain of 0.1 % by PCMI which corresponds to the failure of the CABRI test-Na.1 fuel rod assuming that the radial power factor at the end of the pre-irradiation is about 0.5, the enthalpy increase for the fuel failure in the HFP case is estimated as about 7 cal/gm for the gap-contacted fuel rod and about 72 cal/gm for the fresh fuel rod as shown in Fig. 8. The enthalpy increase for the fuel failure directly depends upon the gap width before the transient.

Therefore, the fuel failure criteria for the high burnup fuel can be conservatively set as the enthalpy increase of as low as 7 cal/gm for the HFP case assuming fuel gap contact while the enthalpy increase of 15 cal/gm for the HZP case. The derived fuel failure limits for the current analysis are shown in Table 2. They were derived based upon the published test results by considering the fuel failure mechanism, and were expressed in terms of the enthalpy increase during the transient for the high burnup fuel which conservatively included those with the burnup higher than 30 MWD/kgU. However, to get the more reliable fuel failure limit, it is clear that more test data need to be generated and evaluated.

2.4 Results Analysis and Discussion

Analysis of the rod ejection accident was performed for two cases of the fuel loading schemes of high burnup and longer cycle length at the EOC (end of cycle) for the HZP and the HFP cases. Detailed results will be described below for the case of the scoping ultra low leakage and 18 month cycle length fuel loading scheme with a peak rod burnup of 68 MWD/kgU.

Figs. 9 and 10 exhibit the enthalpy increase and total enthalpy distribution in the core for the HZP and the HFP cases. The peak enthalpy increase occurred in the position where the control rod was ejected. It can be also seen that the higher burnup fuel has lower potential for the power increase during the rod ejection transient. As the distance from the ejected control rod increases, the magnitude of the enthalpy increase decreases significantly.

Fig. 11 shows the fractional distribution of the fuel burnup in the core where the groups of the 1, 2 and 3 cycle burned fuel regions can be seen. Figs. 12 and 13 show the axial peak enthalpy increase versus the fuel rod burnup for the HZP and the HFP cases, respectively. From these results, the fraction of the fuel failure can be estimated by applying the fuel failure criteria versus burnup. Fig. 14 shows that the distribution of the enthalpy increase is highly skewed to lower value and therefore, the fraction of the fuel failure would not be high during rod ejection accident.

Table 3 summarizes the results for two fuel loading schemes. Estimation of the fuel failure during the rod ejection accident showed 3.6 % fuel failure for the HZP and 1.6 % for the HFP when
the newly derived fuel failure criteria given in Table 2 are applied. Since peak fuel enthalpy is 61.2 cal/gm for the HFP and 52.8 cal/gm for the HZP case, DNB is not expected to occur.

Results of three dimensional analysis of the rod ejection accident also indicated that the core damage design criteria would be satisfied even though the criteria for the fuel dispersion to damage the core integrity also needs to be determined as the more test data for the fuel dispersion accumulate.

Results of the conventional zero dimensional analysis showed that the peak enthalpy of 170 cal/gm for the HZP case and 98 cal/gm for the HFP case, and fuel failure by DNB is just below 10%. When comparing those with the current three dimensional analysis results, the peak enthalpy and the peak enthalpy increase during the transient for the HZP case decreased by a factor of 3.2 and 4.4, respectively.

The conservatism employed in this analysis can be summarized as follows. The core conditions and assumptions used in the transient power calculation are conservative. The assumption of adiabatic enthalpy increase during the transient over-predicts conservatively the enthalpy increase. The derived fuel failure criteria can be considered conservative in that the fuel property degradation at the high burnup of over 60 MWD/kgU occurs as early as 30 MWD/kgU. Therefore, it can be said that the fractional fuel failure during both the HZP and the HFP rod ejection accidents is less than 10% even assuming the conservative fuel failure criteria, and the coolable geometry of the core will be maintained.

3. CONCLUSION

- Fuel behavior under the rod ejection accident in a typical Westinghouse-designed 1,000 MWe PWR plant was analyzed through the three dimensional transient core power calculation with the nodal neutronics code, PANBOX2 and the transient fuel rod analysis code, FRAP-T6.

- Fuel failure design criteria were newly derived, using the simulated RIA test results in the research reactors and the FRAP-T6 prediction. The fuel failure limit was set in terms of not total enthalpy but enthalpy increase, considering that the fuel failure would occur by the pellet-cladding mechanical interaction in the high burnup fuel. However, to set the reliable fuel failure limit, more test data need to be generated and evaluated.

- Results of three-dimensional analysis of the rod ejection accident in the core of the high burnup and longer cycle length fuel loading scheme with the peak rod burnup of over 60 MWD/kgU by keeping the same conservative assumptions of the conventional zero dimensional analysis methodology and applying the newly derived conservative fuel failure criteria showed that the fuel failure would be less than 4% and the peak fuel enthalpy would be 61.2 cal/gm. Therefore, it is expected that the current assumption of 10% fuel failure during the rod ejection accident in the safety analysis report would be still valid and the core coolable geometry would be maintained.

REFERENCES

(2) F. Schmitz et al., “Investigation of the behavior of high burnup PWR fuel under RIA conditions in the CABRI test reactor”, 22nd Water Reactor Safety Information Meeting, October 24-26, 1994.
Table 1. Core conditions and assumptions in the rod ejection accident analysis

<table>
<thead>
<tr>
<th>Item</th>
<th>Core conditions / Assumptions</th>
</tr>
</thead>
<tbody>
<tr>
<td>-Burnup step</td>
<td>-End of cycle</td>
</tr>
<tr>
<td>-Rod ejection time</td>
<td>-0.1 sec</td>
</tr>
<tr>
<td>-Initial power level</td>
<td>-HZP (1 watt), HFP (102% full power)</td>
</tr>
<tr>
<td>-Initial bank position</td>
<td>-Rod insertion limit - 12 steps (at given power level)</td>
</tr>
<tr>
<td>-Final bank position</td>
<td>-Only the stuck rod is ejected from initial bank position</td>
</tr>
<tr>
<td>-Axial offset</td>
<td>-For HFP : reference axial offset + right limit of the target axial offset</td>
</tr>
<tr>
<td>-Xenon status</td>
<td>-Bottom peaked xenon distribution is used</td>
</tr>
<tr>
<td>-Absorption cross section of the ejected control rod</td>
<td></td>
</tr>
<tr>
<td>-Delayed neutron fraction</td>
<td>-Decreased by 10%</td>
</tr>
</tbody>
</table>

Table 2. Derived fuel failure limits based upon the published test results

<table>
<thead>
<tr>
<th>Initial condition</th>
<th>Burnup (MWD/kgU)</th>
<th>Fuel failure limit</th>
</tr>
</thead>
<tbody>
<tr>
<td>HZP</td>
<td>0-30</td>
<td>total fuel enthalpy 85 cal/gm or DNB</td>
</tr>
<tr>
<td></td>
<td>&gt;30</td>
<td>fuel enthalpy increase 15 cal/gm</td>
</tr>
<tr>
<td>HFP</td>
<td>0-30</td>
<td>total fuel enthalpy 85 cal/gm or DNB</td>
</tr>
<tr>
<td></td>
<td>&gt;30</td>
<td>fuel enthalpy increase 7 cal/gm</td>
</tr>
</tbody>
</table>

Table 3. Summary of the results performed for the two PWR fuel loading schemes

<table>
<thead>
<tr>
<th>Fuel loading scheme</th>
<th>Initial condition</th>
<th>Ejected rod worth ($)</th>
<th>Peak total fuel enthalpy (cal/gm) / burnup (MWD/kgU)</th>
<th>Peak enthalpy increase (cal/gm) / burnup (MWD/kgU)</th>
<th>Fuel failure (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Case 1</td>
<td>HZP</td>
<td>1.53</td>
<td>51.2 / 23.7</td>
<td>32.6 / 23.7</td>
<td>2.5</td>
</tr>
<tr>
<td></td>
<td>HFP</td>
<td>0.21</td>
<td>58.7 / 23.6</td>
<td>14.2 / 37.6</td>
<td>1.6</td>
</tr>
<tr>
<td>Case 2</td>
<td>HZP</td>
<td>1.58</td>
<td>52.8 / 25.9</td>
<td>34.2 / 25.9</td>
<td>3.6</td>
</tr>
<tr>
<td></td>
<td>HFP</td>
<td>0.15</td>
<td>61.2 / 26.7</td>
<td>19.8 / 25.5</td>
<td>0.9</td>
</tr>
</tbody>
</table>

(*) Case 1: low leakage and 18 month cycle length scheme with a peak rod burnup of 54 MWD/kgU
Case 2: ultra low leakage and 18 month cycle length scheme with a peak rod burnup of 68 MWD/kgU
Fig. 3 Comparison of the enthalpy increase predicted by the FRAP-T6 with that by the power integration method for the HZP case.

Fig. 4 Comparison of the enthalpy increase predicted by the FRAP-T6 with that by the power integration method for the HFP case.

Fig. 5 Predicted transient fuel behavior during the HZP rod ejection transient.

Fig. 6 Predicted transient fuel behavior during the HFP rod ejection transient.
Fig. 7 Initial fuel enthalpy as a function of the fuel rod power level.

Fig. 8 Cladding strain versus enthalpy increase for the HZP and HFP cases.
### Fuel Enthalpy

| 1.075 | 1.567 | 0.785 | 1.878 | 1.819 | 1.972 | 1.504 | 2.044 | 2.867 | 5.891 |
| 54.89 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 |
| 19.15 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 |
| 19.40 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 |
| 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 |
| 1.588 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 |
| 51.47 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 |
| 19.24 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 |
| 19.80 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 |

#### Fuel Enthalpy Increase

| 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 |
| 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 |
| 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 |
| 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 | 0.000 |

Fig. 9 Fuel enthalpy increase distribution in the core after the HZP rod ejection transient at EOC
Fig. 10 Fuel enthalpy increase distribution in the core after the HFP rod ejection transient at EOC
Fig. 11 Fractional distribution of the fuel burnup in the core

Fig. 12 Fuel enthalpy increase versus fuel rod burnup after the HZP rod ejection transient at EOC

Fig. 13 Fuel enthalpy increase versus fuel rod burnup after the HFP rod ejection transient at EOC

Fig. 14 Fractional distribution of fuel enthalpy increase after the HZP rod ejection transient at EOC
Introduction:

The Electric Power Research Institute (EPRI) has performed analyses to understand the significance of recent results of experiments associated with reactivity insertion accidents (RIA) on high burnup fuel integrity[1] as well as the applicability of these results to currently operating LWR's. Part of this effort is devoted to the development of realistic models of current PWR and BWR cores, in order to determine if the test conditions[1], specifically the power pulse, are typical of the event in a LWR.

Historically, a fuel enthalpy threshold level of about 170 cal/g has been used as the figure of merit for postulated fuel failure. In the current safety analysis process for reactivity insertion accidents, any fuel rods calculated as having exceeded this specified enthalpy level are considered to have failed. A recent test[1] has raised concerns that the existing failure criteria may not be appropriate for high-burnup LWR fuel during a RIA. Based on this test, the threshold for fuel integrity may be lowered by a significant amount. This would directly affect the licensing of longer operating cycles and extended fuel exposures which, for economic reasons, are now being pursued by most utilities worldwide.

The reactivity insertion accidents considered in this paper are the rod ejection accident (REA) in a pressurized water reactor (PWR) core and the control rod drop accident (CRDA) in a boiling water reactor (BWR) core. These are highly energetic and rapid transients with an extremely small probability of occurrence; nevertheless, they are design basis accidents events that must be considered in the plant safety analysis.

For safety analysis purposes, the RIA for PWRs has historically been analyzed with point kinetics model using core wide coefficients having a high degree of conservatism. This approach was necessary because of the crude simulation tools and limited capability of the computers initially available. Despite the development of modern simulation codes (fully three-dimensional capability) and present day workstations, most of the highly conservative assumptions are still being applied. In this paper, the difference between treating a RIA transient with best-estimate (realistic) and conservative limiting assumptions is explored.
Validation of Code and Models:

The RIA analyses were performed using CORETRAN, a combination of the ARROTTA[2] and VIPRE-02[3] codes, which allows a full three-dimensional time-dependent RIA simulation for both PWR and BWR cores.

CORETRAN’s kinetic module is basically the ARROTTA code, and uses the analytical nodal method with explicit representation of the reflector/baffle regions. It allows three-dimensional, time-dependent calculations of core events. ARROTTA has been the subject of several validation efforts. Duke Power Company[4] obtained a US Nuclear Regulatory Commission (NRC) safety evaluation report (SER) for the use of ARROTTA when applied to rod ejection accidents for their Catawba plants. EPRI has performed cross comparisons of ARROTTA and the Combustion Engineering[5] code HERMITE for a specified rod ejection event. Taiwan Power Company[6] used ARROTTA to compare with equivalent nodal codes and operating data from the Maanshan reactor. These validation efforts have demonstrated the capability of the ARROTTA code to simulate a wide variety of steady-state and transient core conditions.

CORETRAN’s thermal-hydraulic module is basically the VIPRE-02 code which can be applied using either a three-equation model or a six-equation model. CORETRAN also has the ARROTTA closed channel thermal hydraulic model as an option. CORETRAN can perform very detailed three-dimensional thermal-hydraulics calculations using a wide range of calculations with the necessary degree of sophistication. The three-equation VIPRE-01 Code (which is the VIPRE-02 parent code) has been reviewed by the NRC and a SER issued for its use for thermal margin calculations for both BWR and PWR applications.

With the support of cooperating electric utilities, full-core three-dimensional models of a PWR core and a BWR core were prepared:

- the PWR core model represents a Westinghouse four-loop core on a 24 months equilibrium cycle. The core can be simulated at beginning-of-cycle or end-of-cycle conditions.

- the BWR core model represents a General Electric BWR-5 core on a 12 months equilibrium cycle. The core can be simulated at beginning-of-cycle or end-of-cycle, cold or hot conditions.

The models used in the RIA analyses were validated against steady-state data provided by the utilities. Before proceeding with any RIA analyses, the time step and radial mesh sizes that would guarantee convergence was determined as:

<table>
<thead>
<tr>
<th>PWR model</th>
<th>BWR model</th>
</tr>
</thead>
<tbody>
<tr>
<td>radial mesh: 1 node per assembly</td>
<td>radial mesh: 1 node per bundle</td>
</tr>
<tr>
<td>number of axial planes: 20</td>
<td>number of axial planes: 27</td>
</tr>
<tr>
<td>time step: 1 ms</td>
<td>time step: 5 ms</td>
</tr>
</tbody>
</table>
Unless otherwise mentioned, all the RIA events considered in this document were simulated with the choices of temporal and spatial meshing specified above. Note that the bottom and top reflectors are part of the axial mesh.

Examples of these parametric study, are given in Figures 1 through 4 which show results obtained when simulating a PWR REA and a BWR CRDA event with different time-step sizes. From these figures one can observe that the use of larger time steps is conservative. An interesting observation that came out of this parametric study was that the time step acceptable for the BWR calculations is bigger than the acceptable one for the PWR calculations. The main reason for this difference is that for a PWR event, the control rod is ejected from the core in approximately 100 ms; whereas for a BWR event it takes approximately 4 sec for a fully inserted rod to be dropped out of the core (assuming control rod speed of 3.11 ft/sec as limited by the control rod velocity limitor).

PWR Analysis:

The PWR rod ejection accident is described[7] as the mechanical failure of a control rod mechanism housing such that the reactor coolant system pressure ejects a control rod assembly. The reactor may become prompt critical and power will rise rapidly until the negative reactivity feedback due to the Doppler effect terminates the power rise within a few hundredths of a second. The energy released may be sufficient to cause fuel damage.

Even though PWR's are not commonly envisioned operating with inserted control rods, these rods (even at full power) can be used in order to shape power distributions (load follow) or to control xenon oscillations. Plant operational technical specifications establish "Control Rod Insertion Limits" which define how much each control rod bank can be inserted into the core at a given power level. The plant is operated to comply with these limits.

The PWR REA's are generally performed for beginning and end of cycle (BOC and EOC) and both at hot zero and full power (HZP and HFP). For the analysis in this paper the full-core operating conditions can be summarized as:

<table>
<thead>
<tr>
<th>hot-zero-power</th>
<th>hot-full-power</th>
</tr>
</thead>
<tbody>
<tr>
<td>power = 0.2 megawatts</td>
<td>power = nominal (3411 megawatts)</td>
</tr>
<tr>
<td>pressure = 2300 psi</td>
<td>pressure = 2300 psi</td>
</tr>
<tr>
<td>flow = 45% of nominal</td>
<td>flow = nominal</td>
</tr>
<tr>
<td>inlet cool. temp. = 560° F</td>
<td>inlet cool. temp. = 560° F</td>
</tr>
</tbody>
</table>

The first step when performing a realistic REA analysis is to identify the rod worth associated with each of the control rods allowed to be inserted in the core at a given operating condition. Figures 5 and 6 show control rod worth at the insertion limits for the end-of-cycle PWR core at both hot-zero-power and hot-full-power operating conditions. From these figures it is established that a $0.88
rod is the highest worth rod to be found at hot-zero-power conditions; whereas a $0.12$ rod is the maximum value at hot-full-power conditions.

The best-estimate value of $0.88$ already indicates that no prompt critical transient can occur; instead the core power will gradually rise until being terminated by a reactor scram. The amount of energy deposited in the adjacent fuel rods will be far less than what would have resulted if the worth of the ejected rod had been $1.30$, which is the value traditionally adopted in PWR REA safety analyses.

In order to compare a best-estimate against a conservative REA simulation, the CORETRAN core model at end-of-cycle and hot-zero-power was modified such that the worth of the $0.88$ rod was increased to $1.30$. This was achieved by modifying the thermal absorption cross-sections in the regions that comprise the assembly that houses the ejected rod.

Figures 7 and 8 show the power and maximum fuel enthalpy for both a best-estimate ($0.88$) and a conservative ($1.30$) REA event. The maximum fuel enthalpy in the realistic analysis ($0.88$) rises slowly, while the conservative case peaks at less than $40$ cal/g.

Figures 9 and 10 show the realistic analysis ($0.12$) results from a hot full power REA. Due to the immediate response from the Doppler, this is a very mild transient with a very gradual maximum enthalpy rise.

**BWR Analysis:**

The BWR control rod drop accident[8] is the postulated separation of the control rod blade from the control rod drive, with the blade sticking in the fully-inserted position while the drive is withdrawn until a high worth control rod pattern is achieved, followed by the dropping of the blade to the control rod drive position. The dropping of a high worth control rod [if the core is already close to criticality] results in a high local reactivity in a small region of the core, and, for large, loosely coupled BWR cores, significant shifts in spatial power generation. The initial rapid power rise is initially limited by Doppler, void and moderation reactivity and finally shutdown is achieved by a high neutron flux initiated scram of all control rods except the dropped rod.

BWR control rod drop events may adversely affect the fuel integrity if they happen at low power (low void) conditions. Once significant voids are generated in the core, the worth of individual rods has decreased to the point where the event is unimportant. Also, if appreciable power is already being generated then the fuel is hot and the Doppler responds sooner.
To limit the control rod worth during start-up, a banked position withdrawal sequence (BPWS)[9] procedure is followed. This is a set of rod-positioning rules that are applicable from cold conditions up to 20% of rated power. Beyond this threshold the BPWS procedures are no longer used or needed. The BPWS is implemented as part of the various hardware-software systems. It specifies or monitors how groups of rods are to be selectively and gradually removed from the core. Consistent with these requirements a "rod pull sheet" is prepared for the operators.

Reference 9 concludes that "the banked position (BP) method of rod withdrawal limits incremental control rod worth to an average value of approximately 0.005 Δk. The highest value of incremental control rod worth calculated for normal core conditions was found to be 0.0083 Δk, where normal core condition is meant to imply no inoperable (bypassed) control rods in the core." The following conservative assumptions form the basis of these results:

- the highest worth rod within a group is dropped first. This is unlikely as the rod pull sheet is generated to move high worth rods at a later time as a way to mitigate the consequence of the CRDA.
- the cold core is at 20° C. This cold temperature would only be possible in the beginning of a core's life and is in fact colder than where most plants are allowed to start pulling rods (around 80° C or 180° F).

For this paper we used the values cited in Reference 9 as our realistic and conservative values for rod worth: 0.005 Δk and 0.0083 Δk, respectively.

BWR CRDA's are generally evaluated for beginning and end of cycle (BOC and EOC) and both at cold and hot zero power (CZP and HZP). For this paper the full-core operating conditions can be summarized as:

<table>
<thead>
<tr>
<th>cold-zero-power</th>
<th>hot-zero-power</th>
</tr>
</thead>
<tbody>
<tr>
<td>power = 1 Kilowatt</td>
<td>power = 1 Megawatt</td>
</tr>
<tr>
<td>pressure = 14.7 psi</td>
<td>pressure = 1000 psi</td>
</tr>
<tr>
<td>flow = 20% of nominal</td>
<td>flow = 20% of nominal</td>
</tr>
<tr>
<td>subcooling = 30° F</td>
<td>subcooling = 2° F</td>
</tr>
</tbody>
</table>

These conditions are derived from the following process of bringing a BWR core to power: (1) once the vessel is closed (atmospheric pressure, 14.7 psi), the recirculation pumps are started at minimum speed (approximately 20% of nominal flow); (2) the core is still fully rodded; (3) once the coolant temperature reaches around 180° F the control rods withdrawal process is initiated; (4) the core is brought to critical condition (This point is defined as the cold-zero-power core condition.); (5) using the nuclear heat now available, the coolant is brought to saturation temperature; (6) the core remains saturated as the core pressure is raised up to operational levels (1000 psi); (7) the recirculation pumps remain at minimum speed. (This point is defined as the hot-zero-power core condition.)
With the core at end-of-cycle hot-zero-power conditions, a survey for the highest worth rod within each group that could bring the core close to a critical state was performed. Figure 11 identifies the rod (from group 7, sequence A) that was determined in this manner. This rod, when fully dropped, is worth $0.0081 \Delta k$ (or $1.50$). In order to reduce the same control rod worth to $0.005 \Delta k$ (or $0.92$), the distance the rod could fall was limited.

Figures 12 and 13 show the difference between running a best-estimate ($0.92$) and a conservative ($1.50$) CRDA event. One can see that the maximum fuel enthalpy in the realistic analysis ($0.92$) rises slowly, while the conservative ($1.50$) case peaks slightly above $40 \text{ cal/g}$.

As part of the cold-zero-power CRDA study the influence of a positive moderator temperature coefficient (MTC) at low temperature conditions was investigated. The results showed that the MTC effect is not important, mostly because the Doppler always terminates the power rise. Highly rodded cores, characteristic of a cold-zero-power condition, have only a smaller positive MTC. The small local moderator temperature increase is felt at the tail end of the power peak and is counteracted by the water itself as it begins boiling (negative void coefficient).

Results from a cold-zero-power CRDA will be reported in a later paper since local boiling in the subcooled region needs to be further studied. Preliminary results indicates that subcooled boiling greatly reduces the severity of the event. However, available "subcooled boiling" models have not been verified for analyses which proceed on a millisecond time scale and at low pressure. Existing experimental work (see reference 10, 11, and 12) indicates that a large amount of void in the local region is possible under similar conditions.

Summary:

In this paper, the analysis of PWR REA and BWR CRDA transients both in a best-estimate (realistic) and in a conservative manner has been performed. For the Westinghouse four-loop PWR studied, the maximum local fuel enthalpy in the realistic analysis rises slowly until the plant is tripped; while the conservative case reaches close to $40 \text{ cal/g}$. Similar results are shown for the BWR-5 CRDA at hot standby conditions where the maximum local fuel enthalpy slowly rises in the realistic analysis while the conservative case peaks slightly above $40 \text{ cal/g}$.

As part of this effort, sensitivity studies were performed to observe the influence of parameters such as: rod ejection time, rod worth, Doppler, void, delayed neutron fraction, and direct moderator heating. For all of the RIA cases run, the rod worth was identified as the controlling parameter.

The difference between the safety analysis using the classical point-kinetics approach and the conservative analysis performed using the 3-dimensional code CORETRAN is shown in Figure 14. Note that the power is nominally a factor of 10 times lower and the pulse is considerably wider. For the realistic case there is essentially no adverse effect except that the plant is tripped.
References:


Figure 1: Core Power
PWR Time Step Study for End of Cycle,
Hot Zero Power, Rod worth $1.30, 1 node per assembly

Figure 2: Maximum Fuel Enthalpy
PWR Time Step Study for End of Cycle,
Hot Zero Power, Rod worth $1.30, 1 node per assembly
Figure 3: Core Power  
BWR Time Step Study for End of Cycle,  
Hot Zero Power, Rod worth $1.50, 1 node per bundle

Figure 4: Maximum Fuel Enthalpy  
BWR Time Step Study for End of Cycle,  
Hot Zero Power, Rod worth $1.50, 1 node per bundle
Figure 7: Rod Ejection Analysis
Core Power for Westinghouse 4-Loop PWR
End of Cycle, Hot Zero Power, Rod worth study

Conservative $1.30 rod worth
Core maximum $0.88 rod worth

Time (Seconds)

Figure 8: Rod Ejection Analysis
Maximum Fuel Enthalphy for Westinghouse 4-Loop PWR
End of Cycle, Hot Zero Power, Rod worth study

Conservative $1.30 rod worth
Core maximum $0.88 rod worth

Time (Seconds)
Figure 9: Rod Ejection Analysis
Core Power for Westinghouse 4-Loop PWR
End of Cycle, Hot Full Power, Rod worth $0.12

![Power vs. Time Graph]

Figure 10: Rod Ejection Analysis
Maximum Fuel Enthalpy for Westinghouse 4-Loop PWR
End of Cycle, Hot Full Power, Rod worth $0.12

![Maximum Fuel Enthalpy Graph]
Figure 11
BWR-5 EOC HZP
Control Rod Positions
at Onset of CRDA

- Sequence A
  - groups 1-6 out
  - groups 7-10 in

- 0 notches → rod fully inserted
- 48 notches → rod fully withdrawn

Figure 12: BWR-5 Control Rod Drop Analysis
Core Power for End of Cycle,
Hot Zero Power, Rod Worth Study

- Conservative $1.50 Rod Worth
- Realistic $0.92 Rod Worth
Figure 13 BWR-5 Control Rod Drop Analysis
Maximum Fuel Enthalpy for End of Cycle, Hot Zero Power, Rod Worth Study

Conservative $1.50 rod worth
Realistic $0.92 rod worth

Figure 14
Comparison of Existing Licensing Analyses with Conservative 3-D Analyses.
Methodology and Results of RIA Studies at Siemens

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Siemens AG, Power Generation Group (KWU), Germany
J. L. Maryott, R. A. Copeland
Siemens Power Corp. (SPC), Nuclear Division, WA, USA

Introduction

Siemens acts as a turnkey-constructor of nuclear power plants, as a fuel vendor, and as a supplier of plant, core and fuel related software services for BWR as well as PWR plants. In the ongoing discussion about burnup dependent enthalpy thresholds for rod failures in reactivity-initiated accidents (RIA), that was initiated by recent experiments in France /1/ and Japan /2/, we feel the twofold challenge to provide realistic – i.e. best estimate but covering a wide range of cases – RIA analysis for both types of plants using state of the art methods. We meet this challenge not only for plants constructed by Siemens but also for those of other vendors. Thus the manifold tasks – summarized in table 1 – are even increased by the fact that different types of plants show different behaviour under postulated RIA events and that different licensing authorities have different approaches to the problem.

<table>
<thead>
<tr>
<th>Type of plant</th>
<th>Limiting RIA event</th>
<th>Computer code used for realistic evaluation</th>
<th>Plant vendor</th>
<th>Remarks, special boundary conditions important for RIA</th>
</tr>
</thead>
<tbody>
<tr>
<td>BWR</td>
<td>rod drop accident (RDA)</td>
<td>RAMONA-3B</td>
<td>Siemens KWU</td>
<td>only 21 cm rod drop due to special control rod drive hardware; start up sequence; UO₂ and UO₂/PuMOX mixed cores</td>
</tr>
<tr>
<td></td>
<td></td>
<td>RAMONA-MB</td>
<td>GE</td>
<td>control rod velocity limiter; banked position withdrawal sequence, rod worth minimizer</td>
</tr>
<tr>
<td></td>
<td></td>
<td>RAMONA-3B</td>
<td>ABB</td>
<td>start up sequence</td>
</tr>
<tr>
<td>PWR</td>
<td>rod ejection accident (REA)</td>
<td>PANBOX2</td>
<td>Siemens KWU</td>
<td>surveillance and control of power density distribution, UO₂ and UO₂/PuMOX mixed cores</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td>Westinghouse</td>
<td></td>
</tr>
</tbody>
</table>

Table 1: Kind of RIA analysis performed by Siemens

The first prerequisite – the availability of methods that yield realistic results – is accomplished since in the past few years considerable efforts have been made at many research...
centers and industrial companies to develop efficient computer codes for the solution of space-time kinetics problems in nuclear power reactors \cite{3}. Different areas of analysis – reactor physics, core thermal hydraulics and plant dynamics – are now integrated to increase the accuracy of simulation over that obtained from imposing conservative boundary conditions at the interfaces. The particular methods used by Siemens are RAMONA \cite{4} for BWRs and PANBOX2 \cite{5} for PWRs.

In this contribution some results of realistic RIA calculations are presented. The main focus is the analysis of RIA events in Siemens plants, the rod drop accident (RDA) in BWRs and the rod ejection accident (REA) in PWRs. This analysis is performed presently for the German utility group VDEW in order to have a sound base in the ongoing discussion on enthalpy limits. Results for other vendor plants are shortly discussed too.

**BWR RDA analysis**

**Methods**

In contrast to the U.S. regulations, in Germany it is common practice of analysis of postulated RIA events in BWRs to demonstrate that a "no rod failure" criterion can be fulfilled which up to now was assumed to be independent of burnup. In Siemens BWRs a system of clamps and notches, that are fixed in the control rod duct, limits the possible drop of a control rod to a maximum of 21 cm. Considering the still effective high enthalpy threshold of 170 cal/g the analysis of this "mini" rod drop could be performed by means of a simple adiabatic method based on point kinetics coupled to steady state peaking factors.

For those cases where the conservatism of this simple method is excessive, Siemens chose RAMONA–3B as a state-of-the-art 3-D transient core simulator. In fig.1 the peak enthalpy values that were obtained in the recent years using the point kinetics, adiabatic method are shown. For some of the cases RAMONA–3B analysis was performed as well. The simple method yields enthalpy values that are about 1.8 times higher than the realistic RAMONA results.

![Figure 1: Results of point kinetics, adiabatic methods and of RAMONA calculations of RDA in Siemens BWR](image)

Siemens uses MICROBURN–B \cite{6} – a two group code with microscopic burnup – as steady state core simulator. The oncoming version of this code will use advanced nodal methods to obtain the flux solution. Siemens Power Corporation (SPC) developed RAMONA–MB, that uses a kinetics method that is fully compatible with the steady state solution and uses the same nuclear cross sections and local peaking data. The system thermal hydraulics model is taken from the RAMONA–3B code. The introduction of RAMONA–MB will further improve the handling and the consistency of the Siemens code system.
Boundary conditions and results for Siemens plants

Postulating a rod drop accident with significant enthalpy increase in a Siemens BWR means to assume the highly unlikely coincidence of several events:

- The core is near criticality
- A control rod gets stuck during withdrawal, thereby disconnecting from its drive.
- The weight sensing device at the control rod drive fails.
- The drive is further withdrawn.
- The stuck rod gets loose again and falls out of the core (maximum 21 cm).
- In order to insert significant reactivity, the rod has to get stuck at a radial and axial position where the differential rod worth is high enough to cause a prompt supercritical transient.
- The neutron flux excursion is very localised so that high relative peaking factors occur.

Especially the last two conditions can only be fulfilled assuming the dense rod patterns and the essentially unvoided conditions during reactor startup. Since the normal rod withdrawal sequence is designed such that the worth of the withdrawn rods is minimized, the withdrawal of an out-of-sequence rod, i.e. an erraneous reactor operation has to be assumed additionally to the above hardware failures. Fig.2 shows that in this case the differential rod worth is very high in the uppermost part of the core and in its steepest range can yield up to 40% of the total rod worth in 21 cm withdrawal length.

![Graph of reactivity insertion](image)

**Figure 2:** Example of reactivity insertion in a BWR by withdrawal of a high worth out-of-sequence rod under cold critical conditions

As can be seen from fig.1 the differential rod worth of the dropping rod is the most important parameter of the rod drop analysis. Obviously the results scatter only narrowly around a line that is a function of this parameter. This is remarkable, since the cases analysed cover a range of cores with different core loading types ("super" low leakage, control cell core, scatter loading), with sizes from 440 to 840 fuel assemblies of different types, from 8–2 (8x8 fuel assembly) to ATRIUM™ 10 (10x10 fuel assembly with internal water channel) with batch averaged discharge exposures from 30 Gwd/t to 50 Gwd/t.

In this situation the task of performing a rod drop analysis covering the whole range of possible core loading situations is solved if the highest possible rod worth can be determined reliably. Having done this, the most pessimistic situation with respect to the combination of high enthalpy and high burnup can be constructed by assembling a control rod cell that is characterized by this rod worth and contains the assembly with the highest burnup in the core.

Fig.3 outlines how a limit on the rod worth can be obtained. For the example of a 12–month equilibrium cycle fueled with ATRIUM™ 10 assemblies with 50 Gwd/t batch average dis-
charge burnup the manifold of total rod worths in cold critical startup rod pattern is plotted vs. the stuck rod shutdown margin associated with the same rod. Both, in-sequence and out-of-sequence rods are considered. The diagram contains BOC and EOC data of a "normal" loading pattern used for core licensing as well as data for some cells that were arbitrarily constructed to achieve high rod worths with high burnup assemblies. Obviously the licensing criterion "minimum shutdown margin has to be higher than 1%" limits possible rod worths to about 3.8%. Furthermore this limit seems to be independent of core burnup. The corresponding reactivity insertion in a 21 cm rod drop is about 1.5%.

Figure 3: Total rod worths of in-sequence and out-of-sequence control rods in cold critical rod patterns and their corresponding cold shutdown margins in stuck rod configuration in an ATRIUM™ 10 equilibrium cycle.

Limiting cells cannot be assembled arbitrarily. Fig.4 shows that the maximum achievable rod worth depends clearly on the average burnup of the control rod cell. Rod worths peak in cells with about 20 GWD/t, dropping almost linearly to higher burnups. For further analyses
cells with about this burnup were constructed, containing fuel assemblies of about 48.8 GWD/t (BOC) and 51.7 GWD/t (EOC). The corresponding "21 cm" cold critical rod worths were 1.6% (BOC) and 1.3% (EOC).

The RAMONA-3B calculations for rod drop within these cells under cold conditions yielded power excursions with half widths of the power pulse of about 60 ms, i.e. significantly higher than the pulse widths of typical "RIA" experiments. The RAMONA enthalpy analysis, that considers only the maximum enthalpy increase in the core was extended to all nodes that are axially and radially adjacent to the dropping rod. Fig.5 shows that the maximum fuel enthalpy of about 95 cal/g occurs in low burnup fuel and drops clearly with burnup. Above 55 GWD/t local burnup there is practically no enthalpy increase though the peak pellet burnup in the core is considerably higher. This is due to the fact that the power excursion is highly localized in the very top of the core, where local burnups are lower or at most equal to the axially averaged burnup. Comparison of the results of the present analysis with the results of the "RIA" experiments performed up to now shows no interference with the range of possible fuel failures.

<table>
<thead>
<tr>
<th>Burnup (GWD/t)</th>
<th>SPERT</th>
<th>PBF</th>
<th>NSRR</th>
<th>CABRI</th>
</tr>
</thead>
<tbody>
<tr>
<td>0</td>
<td>no failure</td>
<td>no failure</td>
<td>no failure</td>
<td>no failure</td>
</tr>
<tr>
<td>50</td>
<td>failure</td>
<td>failure</td>
<td>failure</td>
<td>no failure</td>
</tr>
<tr>
<td>60</td>
<td>failure</td>
<td>failure</td>
<td>failure</td>
<td>no failure</td>
</tr>
</tbody>
</table>

**Figure 5:** Comparison of the results of "RIA" experiments with results of the present covering RAMONA analysis of the "mini" rod drop in German BWRs.

Boundary conditions and results for GE plants

Analysis for typical GE plants were performed at SPC using RAMONA-MB. In GE plants a rod that is dropping from totally in to totally out has to be considered. In order not to violate enthalpy thresholds in a postulated RIA event, several measures are introduced. Firstly, the control rods are equipped with a bottom piece that limits the velocity of a dropping rod and thereby the speed of reactivity increase. Secondly, and more important, a special startup sequence is used, the banked position withdrawal sequence (BPWS). This sequence uses control rod groups withdrawn to intermediate banked positions to reduce possible control rod worths near critical at powers below typically 10 percent power. The BPWS is assured by the Rod Worth Minimizer system at the plant.
Core designs with 24-month cycles fueled with ATRIUM™ 9 and ATRIUM™ 10 reload fuel were analysed. Using conservative assumptions including a single banked group withdrawal, the maximum rod worth was 0.9%. The maximum fuel enthalpy of 57 cal/g occurred in low burnup fuel (< 14 Gwd/t). At a burnup of about 40 Gwd/t the maximum enthalpy was 40 cal/g. There was no significant difference between the types of reload fuel considered.

**PWR REA analysis**

**Methods**

In contrast to standard nodal (coarse-mesh) kinetics applications, in safety calculations local values of power and coolant conditions are needed. To this purpose, in Siemens PWR special kinetics code system PANBOX2 an automatic renodalisation of the corewide channel geometry of its thermalhydraulics module COBRA3-CP /7/ was implemented to include hot subchannels and appropriate subchannel windows surrounding them. This coupling of thermal-hydraulic subchannel analysis with nodal space-time kinetics calculations is an important step towards an even more extensive integration of complex code systems /8/. It is also the basis for improved analysis tools and their automation, indispensable for the efficient and accurate determination of local safety related parameters.

**Boundary conditions and results**

A rod ejection accident (REA) in a PWR leads to a fast reactivity insertion and consequently to a rapid power excursion, so it represents conditions typical for reactivity initiated accidents (RIA). Though the probability of a rod ejection is low it has to be carefully examined with respect to the consequences which are governed by the ejected control rod worth and the resulting power distribution. Typically, a spectrum of REAs has to be investigated covering initial states from hot zero power (HZP) to hot full power (HFP) at BOC and EOC conditions. Control rods are assumed to be at their insertion limits corresponding to the power level under consideration. The surveillance of these insertion limits is integrated in the reactor protection system.

The spectrum of REA analyses for Siemens PWRs with the program system PANBOX2 cover pure Uranium and U/ MOX cores and a large range of burnups by ejection of rods from fuel assemblies with different residence times. Rod ejection time is assumed to be 0.1 seconds.

The three-dimensional transient pin-wise power distribution allows a detailed evaluation of the fuel enthalpy rise, based on hot channel analyses for each fuel assembly. Different locations of the hot channel within a fuel assembly are considered. In the first "standard" method the position of the hot pin defines the location of the hot channel. This position may change during the transient which is automatically traced by the program. The second method uses fixed positions of the hot channels in the eight fuel assemblies surrounding the assembly with the ejected rod. This approach is only relevant for the HFP cases, where the fuel pellets start with different values of the enthalpy, while starting at HZP, the maximum enthalpy rise always occurs within the hot pin. The positions are defined at edges and corners adjacent to the assembly with the ejected rod. Calculations have shown, that, compared to hot pins, these fuel rods may experience higher enthalpy rises because of their direct proximity to the high neutron flux increase. It is also obvious, that it is not necessarily the hot pin with a high initial value in the enthalpy, which shows the highest enthalpy rise. For each hot channel of a fuel assembly the enthalpy histories of all axial nodes are scrutinized and the maximum local fuel pellet enthalpy rise is determined.

A reliable comparison of the fuel enthalpy rise with available "RIA" experiments proves to be difficult, since the pulse widths of these experiments are extremely small compared to REA transients analysed for Siemens PWRs. As illustrated in fig.6, the worst HZP case does not
even initiate a reactor trip and thus shows a continuous enthalpy increase, behaving like a power ramp rather than a RIA. For those cases the enthalpy rise is evaluated after a time of about 1 second, which roughly corresponds to the maximum time interval in which the transients with initiation of trip reach their maximum enthalpy rise. It has to be emphasized that this time is significantly larger than corresponding times of "RIA" experiments.

![Graph showing change of reactor power relative to full power vs. time](image)

**Figure 6**: Change of reactor power relative to full power vs. time in limiting best estimate REA analysis for 1300MW Siemens PWR

This paper focuses on calculations using realistic initial and boundary conditions. The analysis covers a wide spectrum of Siemens PWR loadings and results in maximum reactivity insertion values of about 320 pcm at initial condition HZP and less than 90 pcm at HFP for the whole range of depletions. Starting from HZP, it is obvious that these cases do not have the potential to violate any of the safety relevant criteria, since there is no prompt critical power excursion. The main reason for the reactivity insertion being restricted to values well below 1 $ is the control rod configuration and management scheme of Siemens PWRs in association with the surveillance and control of the power density distribution. Rod worths are strictly limited by the rod insertion limitation. In particular, detailed analyses of the core behavior show fuel enthalpy rises of less than 1 cal/g starting the REA at HZP and of less than 5 cal/g starting the REA at HFP. Hence, the integrated best estimate analyses show large safety margins also for reduced failure thresholds.

Fig. 7 illustrates the maximum fuel enthalpy rise as a function of the maximum local burnup for a 1300 MW Siemens PWR. It comprises the whole spectrum of the analyses, i.e. rods which are ejected from fuel assemblies of first to fourth residence time and burnup states at BOC and EOC.

As is well-known, using conservative boundary conditions with the associated higher ejected rod worths of significantly more than 1 $ at end-of-cycle (EOC) HZP conditions in combination with simple analysis methods, can result in a deposited energy of up to 180 cal/g for fresh fuel. On the other hand, by using a more accurate three-dimensional transient methodology these predictions can be reduced significantly. Within the scope of work of Siemens for other-vendor plants, three-dimensional transient analyses using best estimate initial and boundary conditions with a rod worth of about 1.5 $ have shown a peak total fuel enthalpy rise of about 25 cal/g in low burnup rods.
Summary and conclusions

Simple, highly conservative methods (point kinetics, adiabatic, syntetic) are adequate for the assessment of a "no rod failure" criterion as long as the up to now effective high failure enthalpy thresholds are to be applied. The recently announced results of "RIA" experiments indicating a substantial decrease of rod failure thresholds with increasing burnup, initiated the introduction of more advanced analysis methods comprising "realistic conservatism". Siemens has performed such type of analysis of rod drop/ejection events for many different types of reactors, fuel, and core designs.

In Siemens BWRs the maximum possible "21 cm" cold critical worth of an out-of-sequence rod is about 1.5% and yields a maximum enthalpy rise of 95 cal/g in low burnup fuel and 20 cal/g in fuel with a local burnup higher than 50 GWD/t. The enthalpy deposition is highly localized. In GE designed BWRs, the rod worth is limited by the BPWS rules to about 1%. Rod drop events yield maximum enthalpy rises of about 60 cal/g in low burnup fuel. Again, for the high burnup fuel the maximum deposited enthalpy is about 20 cal/g.

In Siemens PWR plants the maximum realistic rod worth is less than 0.4% at HZP, thus there is no prompt reactivity excursion. The power excursion at HFP is limited by the low in-
serted reactivity of <0.1%. In other vendor plants, realistic EOC, HZP rod worths of 1.5 $ may occur, yielding a peak fuel enthalpy rise of about 25 cal/g. As with the BWRs, the energy deposition is localized in the vicinity of the dropped rod and the deposited enthalpy decreases with burnup.

The major conclusions that are valid for all cases investigated are:

- The dominant characteristic of RIA events is the worth of the control rod.
- In realistic core loadings the existence of a high worth control rod in the proximity of high burnup fuel (>40 GWd/t assembly burnup) is very unlikely.
- If a high worth rod drops/ejects, the energy deposition will be localized and the number of rods seeing significant depositions is limited.
- For all RIA events, the enthalpy in high exposure fuel is significantly less than the enthalpy in low exposure fuel.

The presented application examples result in values for inserted enthalpies of which it can be presumed that they will not conflict with reduced failure thresholds possibly to come.

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/8/ A.Knoll, R.Müller
ANALYZING THE BWR ROD DROP ACCIDENT
IN HIGH-BURNUP CORES

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1This work was performed under the auspices of the Nuclear Regulatory Commission.
EXECUTIVE SUMMARY

This study was undertaken for the U. S. Nuclear Regulatory Commission to determine the fuel enthalpy during a rod drop accident (RDA) for cores with high burnup fuel. The calculations were done with the RAMONA-4B code which models the core with 3-dimensional neutron kinetics and multiple parallel coolant channels. The calculations were done with models for a BWR/4 with fuel bundles having burnups up to 30 GWd/t and also with a model with bundle burnups to 60 GWd/t.

The calculations were done assuming initial conditions at zero power with the coolant 70°C subcooled. The control rod pattern was at 50 percent control density and the rod dropped had a static worth of 950 pcm. The RDA caused a power excursion that was initially terminated by fuel temperature (Doppler) feedback. The power remained at relatively high levels until void feedback and reactor trip reduced the reactivity sufficiently.

The maximum increase in fuel enthalpy in the core was less than 70 cal/g for the medium burnup core which is low relative to existing acceptance criteria for this event. The enthalpy rise was determined not only by the dropped rod worth and magnitude of the feedback but also by the timing of the feedback. With large subcooling, the generation of void feedback is delayed and the fuel enthalpy continues to rise after the initial increase in enthalpy due to the power pulse.

The maximum increase in fuel enthalpy was calculated for the bundles surrounding the dropped rod and then plotted versus the burnup of the node in which the maximum occurred. The results of the calculations were consistent with the expectation that the peak fuel enthalpy in any bundle would be a complicated function of the dropped control rod worth, the distance of the bundle from the dropped rod, and the burnup at that location. This result was found to be the case for both the medium and high burnup cores for which the RDA was calculated.

This paper also discusses potential sources of uncertainty in calculations with high burnup fuel. One source is the "rim" effect which is the extra large peaking of the power distribution at the surface of the pellet. This increases the uncertainty in reactor physics and heat conduction models that assume that the energy deposition has a less peaked spatial distribution. Two other sources of uncertainty are the result of the delayed neutron fraction decreasing with burnup and the positive moderator temperature feedback increasing with burnup. Since these effects tend to increase the severity of the event, an RDA calculation for high burnup fuel will underpredict the fuel enthalpy if the effects are not properly taken into account. Other sources of uncertainty that are important come from the initial conditions chosen for the RDA. This includes the initial control rod pattern as well as the initial thermal-hydraulic conditions.
INTRODUCTION

Reactivity-initiated accidents and certain design-basis transients lead to power excursions which are considered acceptable if they meet specified acceptance criteria. For rapid power excursions, these criteria are based on the energy deposition in the fuel pellet which is approximately equal to the fuel enthalpy. In recent years, experiments have been performed to examine the behavior of high burnup fuel subjected to power pulses. Some fuel has failed at energy depositions that are low relative to the acceptance criteria. Furthermore, other recent studies of high burnup fuel show that property changes, especially in the cladding and at the surface of the pellet, could make the fuel more vulnerable to power pulses. These activities have called into question the current acceptance criteria, and new studies to address this issue have been undertaken by the light water reactor community throughout the world.

The U.S. Nuclear Regulatory Commission (NRC) has expressed its concern regarding the above in two Information Notices that have been issued: (NRC, 1994) and (NRC, 1995). In addition, the NRC sponsored a research program to improve their understanding of the situation and to see if regulatory action is needed. This program has three different thrusts. One is to study the new experimental data being collected in France, Japan, and Russia as well as the data available from measurements made in the past, especially in the U.S. This is intended to enable the NRC to better understand how burnup affects fuel behavior during power excursions. Another is to improve analytical models of fuel behavior so that they are applicable at high burnup. This would enable analytical studies of fuel behavior to be completed. The third thrust is to review the transient/accident analysis that has been done in the past and to perform new calculations that will estimate the amount of energy that can be deposited in high burnup fuel. This paper reports on a part of this last thrust. Specifically, it presents results of RAMONA-4B calculations of the rod drop accident in a boiling water reactor (BWR). This event leads to the largest energy deposition for BWR design basis accidents.

ANALYSIS OF THE BWR ROD DROP ACCIDENT

Description of RAMONA-4B

RAMONA-4B (Wulff, 1984) is a systems transient code for boiling water reactors. The code uses a 3-dimensional neutron kinetics model coupled with a multichannel, 2-phase flow model of the thermal-hydraulics in the reactor vessel. The code is designed to analyze a wide spectrum of BWR core and system transients. The 3-dimensional neutron kinetics makes the code well-suited for predicting transients and accidents where the spatial core power variations are expected to be significant.
The reactor core is modeled with multiple parallel coolant channels and a bypass channel. Each coolant (i.e., thermal-hydraulic) channel is interfaced with one or more fuel bundles. The reactor power, including decay heat, is calculated in 3-dimensional geometry. The fission power calculation takes into account control rod movement (including accidental rod drop and reactor trip) and the feedback throughout the core due to changes in the fuel and coolant temperatures and steam void fraction. Energy deposited directly into the coolant and bypass channels is taken into account. Thermal conduction through the fuel pellet, gas gap, and fuel cladding is modeled to obtain the heat transfer from the fuel to the coolant.

The neutron kinetics model of RAMONA-4B is based on 2-group diffusion theory with up to six delayed neutron precursor groups. Simplifications are made in treating the thermal neutron flux to reduce the formulation to a 1½ group, coarse mesh diffusion model in a 3-dimensional rectangular coordinate system. Neutronic boundary conditions are specified at the axial and radial core periphery.

The thermal-hydraulics of the core region was modeled using 160 thermal-hydraulic channels associated with fuel bundles and one bypass channel representing the area between the bundles. The majority of the thermal-hydraulic channels were "shared" by several neutronic channels. The thermal energy released in those several neutronic channels was collectively deposited into the liquid flowing in that particular thermal-hydraulic channel. Each of the neutronic channels in three rows of bundles adjacent to the core's axis of symmetry had a dedicated thermal-hydraulic channel in order to most accurately represent the thermal-hydraulic reactivity feedback effects (void fraction and moderator and fuel temperature) following a control rod drop.

Two cores were modeled. One model was for a medium burnup core and represented fuel bundles with burnups up to a maximum of approximately 30 GWD/t. The cross sections for this core had been generated using the CASMO code (Ahlin, 1978) for a previous study.
The other model was meant to represent the situation with bundle average burnups up to 60 GWd/t (and, hence, fuel rod burnups of up to approximately 65 GWd/t). Since no data were available to the authors for actual or planned cores with this burnup, a method was used which allowed for the medium burnup data to be extrapolated to produce the high burnup core. New cross sections were generated using the CPM code (Ahlin, 1975). This core model is only an approximation to an actual core. However, it provides sufficient information to test certain hypotheses and add to our understanding of high burnup cores.

Initial Conditions for RDA Analysis

The calculation of rod drop accidents was done for both the medium and high burnup core models. Table 1 contains some of the neutronic and thermal-hydraulic parameters used to describe initial conditions, plant response, and modeling in RAMONA-4B for these calculations.

<table>
<thead>
<tr>
<th>Parameter/Condition</th>
<th>Value/Description</th>
<th>Comment</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fuel bundle maximum burnup</td>
<td>30/60 GWd/t</td>
<td>For medium/high burnup calculations</td>
</tr>
<tr>
<td>Reactor power</td>
<td>3.29 kW (&quot;zero&quot; power)</td>
<td>10^6 of rated power</td>
</tr>
<tr>
<td>Control rod insertion pattern</td>
<td>Checkerboard; 50% control rod density</td>
<td>See Figure 1</td>
</tr>
<tr>
<td>Fraction of energy deposited directly into coolant</td>
<td>0.04</td>
<td>Total for the in-channel and bypass liquid</td>
</tr>
<tr>
<td>Delayed neutron fraction</td>
<td>0.006/0.005</td>
<td>For medium/high burnup calculations</td>
</tr>
<tr>
<td>Xenon inventory</td>
<td>Fully depleted</td>
<td></td>
</tr>
<tr>
<td>Reactor trip setpoint</td>
<td>15% of rated power with 0.2 s delay</td>
<td></td>
</tr>
<tr>
<td>Scram insertion speed</td>
<td>1.2 m/s (3.9 ft/s)</td>
<td></td>
</tr>
<tr>
<td>Control rod drop speed</td>
<td>0.94 m/s (3.1 ft/s)</td>
<td></td>
</tr>
<tr>
<td>System pressure</td>
<td>0.1 MPa</td>
<td>Non-condensible atmosphere</td>
</tr>
<tr>
<td>Liquid temperature</td>
<td>30°C</td>
<td>70°C subcooled</td>
</tr>
<tr>
<td>Core flow rate</td>
<td>3260 kg/s</td>
<td>25% of rated flow</td>
</tr>
</tbody>
</table>
The initial control rod pattern with 50 percent control density, shown in Figure 1, was chosen for several reasons. The most important was that in a study of limited scope, it would be too difficult to search through all of the possible patterns to obtain a pattern with the highest worth dropped control rod or the worst fuel enthalpy increase. BWR reactors use systems that lead to patterns such as those in the banked position withdrawal sequence (Paone, 1977). Not only would one have to go through all the patterns possible using this system but also patterns possible if a single failure criterion was applied. With the 50 percent control density, control rod worths of up to 950 pcm were calculated along the axis of symmetry. This highest worth corresponds to rod worths obtained by other analysts using the banked position withdrawal sequence; and, therefore, it was felt justified to use for the rod drop analysis.

The initial thermal-hydraulic conditions in the reactor corresponded to cold startup. The power was 10\(^6\) of full power, and the core coolant temperature was 30°C. This represented 70°C subcooling at atmospheric pressure which was assumed to be the system pressure. This delays the onset of steam generation caused by the RDA and, therefore, the addition of negative void reactivity feedback which tends to mitigate the accident. The single-phase coolant decreases the heat transfer to the coolant relative to the case with 2-phase flow. This has the effect of keeping the fuel temperature (and fuel enthalpy) higher; and, as with the void reactivity, this is in the direction so as to make the results more severe, i.e., more limiting. The higher fuel temperature also increases the fuel temperature reactivity feedback which limits the severity of the accident, but this is expected to be a smaller effect. The high subcooling at low initial temperature means that coolant/moderator temperature reactivity feedback can be important. For a BWR at cold conditions, the feedback is positive and, therefore, can exacerbate the power excursion.

In most BWRs, the reactor becomes critical when only approximately one fourth of the control rods are withdrawn. Hence, cold conditions would correspond to higher control rod densities than the 50 percent used in this study. At 50 percent control density, higher temperatures and pressures are expected as the power would have increased from its initial level at the cold condition. Best estimate calculations would have to take into account the change in thermal-hydraulic conditions with changing control rod patterns. The thermal-hydraulic conditions control the positive moderator feedback, the heat transfer to the coolant, and the onset of negative void feedback.

The initial conditions for the medium burnup core results in a (high) axial peaking factor of 3.5 at the top of the core—typical of shutdown conditions in a BWR. This axial peaking tends to increase the rate of reactivity insertion when the rod drops out of the core. This means that the power increases rapidly while the control rod is still in the top half of the core.
Results for a Medium Burnup Core

The accident was initiated at time zero with CR #14 (see Figure 1) dropping at a speed of 0.94 m/s (3.1 ft/s). The prompt power excursion started at about one second, as can be seen in Figure 2 which shows the power during the transient on a logarithmic scale relative to nominal, or rated, power. The power increases more than six decades which is typical for this type of RDA.

The figure also shows the position of the control rod which is initially completely inserted. As can be seen, when the tip of the control rod traveled only three to four feet through the core, sufficient reactivity had been inserted to cause the power excursion which, in turn, was terminated by fuel temperature feedback (primarily due to the Doppler effect). This means that when realistic control patterns are considered in setting up conditions for the RDA, it is only necessary that the control rod drive mechanism be withdrawn halfway out of the core in order to set the stage for the assumption that the corresponding blade has been decoupled and has stuck so that it can later drop to the position of the drive mechanism.

The reactor power reached a peak value of approximately 2.4 of nominal power at about 1.3 seconds. At that time, the negative Doppler reactivity feedback is large enough so that the power excursion is terminated. The history of the different reactivity feedback components is shown in Figure 3 which also shows the power excursion on a linear scale. This figure shows that the accident can be separated into four major phases. In the first phase, reactivity is being inserted due to withdrawal of the dropped control rod. The second phase starts when the power surge is reversed due to fuel temperature (Doppler) reactivity. The third phase covers the period from the initiation of boiling in the core and its associated negative reactivity. The fourth phase occurs when the void feedback and scram become effective enough to completely shut down the core.

The plot of reactivity effects shows that the control rod worth germane to this event is approximately 750 pcm. This is 80 percent of the total static worth of the rod and primarily is the result of the fact that the rod does not withdraw all the way before the event is terminated. The figure also shows the positive reactivity feedback due to moderator heatup.

The axial power distribution also changes during the transient, but because it is peaked at the top of the core initially and the rod is dropping from the top of the core, the axial node with the peak power remains at the top (node 21 where node 24 is at the top of the core).

Although core-average thermal-hydraulic parameters do not change significantly, the localized values change dramatically. The coolant temperature rises to saturation and then boiling begins in the bundles surrounding the dropped rod. This is primarily the result of direct energy deposition; although after approximately one second, heat transfer across the cladding also
becomes important. At the incipience of boiling, RAMONA-4B predicted flow oscillations and reversal in the hot channels. This in turn led to critical heat flux in a number of channels.

Boiling introduces negative reactivity and, therefore, could be important in mitigating the total enthalpy increase. In other situations, with little or no subcooling, boiling could begin very soon into the transient and reduce the power excursion and the immediate enthalpy increase. In these situations, there is a burden on the accuracy of the thermal-hydraulics model being used. Although it is clear that a certain amount of energy deposition in the coolant leads to boiling, the timing could be important, and current void generation models are based on experiments that do not mimic the dynamic conditions found during a RDA.

The results of most interest in this study are for fuel enthalpy (defined as the pellet radial average at any location in the core) as that is the parameter which is currently used to determine the acceptance limit for the RDA (280 cal/g in the U.S.) and the condition for fuel failure (170 cal/g in the U.S. for BWRs at low or zero power) for the purpose of calculating the radiological response. In the past, only the peak fuel enthalpy throughout the core has been of interest in licensing calculations, i.e., the maximum in both space and time. However, in the present study, it was of interest to understand the peak during the event as a function of the burnup of the fuel and that requires knowing the peak enthalpy in all the nodes in the region around the dropped rod. In the following, bundle enthalpy is considered recognizing that if the results could be translated to an individual rod within a bundle, the fuel enthalpy would be higher. In order to know how much higher, one would have to do detailed calculations for the region surrounding the dropped rod. The hottest rod in a steady state might have a power 10-15 percent above the bundle average, but in the transient situation, the peaking could be quite higher.

Figure 4 shows the maximum fuel bundle enthalpy in three neutronic channels (fuel bundles) as a function of time. The maximum in time occurs in Channel 27, which is one of the bundles directly adjacent to the dropped control rod (CR #14 in Figure 4.1). Channel 56 is diagonally adjacent to Channel 27, and Channel 89 is one pitch removed from Channel 27. The predicted enthalpies are for an interval of 15.9 cm (6.3 in) corresponding to axial node 21 which is the node with maximum fission power. The legend shows the bundle burnup at the node in the bundle for which the enthalpy is a maximum. Reactor power history is also shown on the figure.

There are three distinct phases in the enthalpy plots: (1) prompt heatup due to the fission power excursion, (2) continuing fission power heatup, and (3) shutdown cool-off. Observation of the enthalpy curves indicates that in this particular calculation, the amount of prompt heatup is roughly equal to the fission power heatup. This results from the initial conditions, mainly from the high initial moderator subcooling which delays bulk boiling in the core—an important factor responsible for shutting down the fission reaction by introducing large negative void reactivity. A lower initial coolant subcooling would result in a lower maximum fuel enthalpy reached during the accident. Note that the separation of the fuel enthalpy increase into the first two
phases may become particularly important if studies of fuel behavior in the future lead to acceptance criteria that are based on both the initial fuel enthalpy rise and the ultimate value.

The peak fuel enthalpy for this event (see Figure 4) is less than 70 cal/g which is considerably below the current values of interest from a licensing point of view. However, for this study, it was of interest to consider the fuel enthalpy as a function of burnup for a given RDA. Figure 5 shows enthalpy versus burnup not only for the three bundles used to generate Figure 4 but rather for all 16 bundles (identified by number on the graph) of most interest surrounding the position of the dropped rod. The figure shows the orientation of these bundles relative to the dropped rod position of CR #14 which is between bundles 27 and 28. The crosses indicate control rods initially inserted.

These results do not indicate a simple correlation between fuel enthalpy and burnup. Rather, they suggest that for the given rod worth, the peak fuel enthalpy in a bundle is a complex function of factors, such as the distance of the bundle from the dropped rod and the burnup of the fuel. In other cases for different control rod worths, the enthalpy in a given bundle could be higher or lower depending on the specific circumstances.

This conclusion is probably valid in spite of the fact that there are several other factors influencing Figure 5—namely, that (1) bundles 30 and 60 are on the core periphery and, therefore, the power surge is mitigated by the neutron leakage into the reflector and (2) the bundles with burnups of about 5 GWd/t have reactivities impacted by the burnout of gadolinium and, therefore, cannot be expected to have the same burnup dependence as bundles with higher burnups where gadolinium is no longer an important factor.

Results for a Pseudo High Burnup Core

The psuedo high burnup core model was used to calculate the effect of dropping CR #14 from a control rod pattern corresponding to 50 percent control rod density. The power versus time is shown in Figure 6 on a logarithmic scale. The behavior is similar to that for the medium burnup case except that the peak power is higher. Although the fuel has a higher burnup in this case, the reactivity is not necessarily lower. More reactivity is designed into the fuel so that the reactor can continue to produce power at the higher burnup. Therefore, it is not surprising that results for the two burnup cases are similar.

The results for maximum fuel enthalpy versus burnup are shown in Figure 7 for the bundles surrounding the position of the dropped rod. Again, there is no clear correlation between burnup and enthalpy, and the conclusions discussed above seem to apply here as well, i.e., that the enthalpy in any node depends on control rod worth, distance from the rod and also on burnup. In this figure and in Figure 5 for the medium burnup core, only the axial node with the peak enthalpy has been considered for a given bundle. Since the bundle burnup will be higher at
nodes that are closer to the center of the core, if these additional nodes were added to the plot, they would show points at higher burnup and lower fuel enthalpy relative to each of the points on the present plot. This would tend to create more points on the graph to the right and down from existing points. However, the nodes further away from the center (e.g., nodes 23 and 24) would have lower enthalpy and lower burnup adding points to the left and down from the existing points. These additional axial points would, therefore, not be expected to reveal any trends and would not negate the possibility of relatively high enthalpy in a high burnup node if it were close to a high worth dropped control rod.

Sources of Uncertainty in RDA Analysis

There are two general sources of uncertainty: (1) the methodology and (2) the assumptions used to define the reactor state. The methodology consists of the computer models and the values of the neutronic and thermal-hydraulic parameters that are used in those models. The validation of computer codes for application to the rod drop accident has always been a difficult matter. Since there have never been any rod drop accidents in a BWR, no data exists to directly assess the uncertainty in the calculated fuel enthalpy during a rod drop accident. Instead, the approach in the past has been to generally validate the computer codes and then to use a conservative approach to determine the margin to the acceptance limits for the rod drop accident. The conservative approach biases the assumptions used to define the reactor state so that the calculated peak fuel enthalpy is maximized.

Although this has been an adequate practice in the past, it will be important in the future to provide an estimate of uncertainty if either the margin between expected fuel failure and calculated fuel enthalpy becomes much smaller than is currently the case or if calculations are done using a best-estimate approach rather than a conservative approach. It will then be important to know the sources of important uncertainties within the models and what impact these have on the uncertainty in fuel enthalpy in a given bundle.

One source of uncertainty is due to the "rim" effect in high burnup fuel. In general, the power distribution through a pellet is peaked at the surface due to self-shielding. This causes the plutonium concentration to grow at the surface. This effect accelerates with time so that the power and the plutonium distributions become highly peaked in a small region at the rim [see, for example, (Lassmann, 1994)]. Reactor physics models that generate cross sections make assumptions about the temperature and power distributions across the pellet. With the rim effect, these assumptions may not be as valid and the uncertainty in results may increase. In addition, the uncertainty may increase due to the need to include more actinides in the models.

The change in composition with burnup influences the thermal properties of the pellet. The rim effect introduces a spatial distribution of properties that may become important. Furthermore,
the uncertainty in calculations may increase with heat conduction models that do not account for the peaked spatial distribution of energy deposition in the pellet.

Two physics properties that may become more important with high burnup are the effect of the delayed neutron fraction (\(\beta\)) and moderator temperature feedback. The power excursion during an RDA is made worse when the delayed neutron fraction becomes smaller. The delayed neutron fraction decreases with burnup, and the ideal model would allow for the spatial distribution of \(\beta\) to account for the burnup throughout the core.

The moderator temperature feedback is positive when the moderator is relatively cold. The effect is made worse if there is significant subcooling. Again, the effect becomes stronger with burnup, i.e., it is more important to model the effect for high burnup cores. This effect was somewhat quantified by redoing the calculation of the RDA for the medium burnup core with no moderator temperature feedback. The elimination of moderator temperature feedback had no significant effect on the initial power pulse and fuel enthalpy increase, but it did decrease the maximum enthalpy by approximately 5 cal/g. Since the moderator temperature reactivity coefficient is linear in the burnup range from 22 GWD/t (the burnup of the node with maximum enthalpy) to a high burnup value of 66 GWD/t, it is reasonable to expect that the effect may be on the order of three times as large or 15 cal/g for high burnup fuel.

Several other sources of uncertainty have been discussed above in the context of the assumptions used to model the initial conditions. The assumed initial control rod pattern (and the core design) determines the rod worth. The assumed initial thermal-hydraulic conditions determine the moderator feedback and the timing of negative void feedback. In a best-estimate calculation, it is necessary to take into account the withdrawal sequence being used at a particular plant, the possibility of an error in that withdrawal, and the corresponding thermal-hydraulic conditions. It is difficult to find a single worst initial condition because the highest worth rod may not lead to the most limiting fuel enthalpy if the acceptance criterion is based on burnup. Nevertheless, it should be possible to identify the leading contenders for worst initial condition so that only a few of the hundreds of theoretically possible accident situations would have to be calculated.

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U. S. Nuclear Regulatory Commission, "Reactivity Insertion Transient and Accident Limits for High Burnup Fuel," NRC Information Notice 94-64, Supplement 1, April 6, 1995.

Figure 1  Initial Control Rod Pattern for RDA Analysis

Unshaded core cells: control blade withdrawn
Shaded core cells: control blade inserted
Figure 2  Power and Rod Position During RDA (Medium Burnup Case)
Figure 3: Reactivity Components During RDA (Medium Burnup Case)
Figure 5  Maximum Fuel Bundle Enthalpy vs. Burnup (Medium Burnup Case)
Figure 6: Power During RDA (Pseudo High Bump Case)
Figure 7  Maximum Fuel Bundle Enthalpy vs. Burnup (Pseudo High Burnup Case)
Investigations Related to Increased Safety Requirements for Reactivity Initiated Accidents

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Abstract

The results of recently performed reactivity initiated accident tests indicate that lower enthalpy limits should be used for high burnup fuel. For this reason the Swiss Federal Nuclear Safety Inspectorate proposed provisional increased safety requirements, including burnup dependent criteria for cladding failure. Some aspects of the impact of these increased safety requirements on the analysis of reactivity initiated accidents were investigated by the Paul Scherrer Institut for a rod drop accident in a BWR/6 plant. This paper discusses the new provisional criteria and the rod drop accident analysis.

1 Introduction

The results of recent reactivity initiated accident (RIA) tests performed in France and in Japan with pellet-average burnups between 50 and 63 MWd/kgU indicate that lower enthalpy limits should be used for high burnup fuel. Therefore, in a letter of November 1994 to the operators of all Swiss nuclear power plants the Swiss Federal Nuclear Safety Inspectorate (HSK) requested plant- and cycle-specific analyses of the rod ejection accident or the rod drop accident (RDA), taking into account the recent high burnup RIA test results and the actual burnup distributions in the plant. Pending further experimental and analysis results, HSK proposed new provisional licensing criteria for RIAs in this letter.

Some aspects of the impact of these increased safety requirements on the analysis of RIAs were investigated by the Paul Scherrer Institut (PSI) for a beyond-design-basis RDA in the Leibstadt BWR/6 plant. For hot zero power and end of cycle conditions in this reactor, RDA cases for different "dropped" rods were calculated with a transient code using 3D neutronics.
While this study considered a specific reactor, it is not a safety analysis, but is rather
an investigation of methods and trends which are significant for a consideration of nodal
dependent enthalpies instead of just the peak enthalpy in the core. A systematic analysis
to determine the uncertainties which should be applied to the calculated results was not
performed. The best-estimate values for space-dependent enthalpies resulting from the
analysis were compared with the new safety criteria. Since this is an initial study for a
specific case, the general conclusions which can be drawn are limited.

The provisional RIA licensing criteria are discussed in section 2. Section 3 describes the
calculational methods employed and assumptions made for the analysis. Results for static
and transient calculations are presented and discussed in sections 4 and 5, respectively,
and conclusions are contained in section 6.

2 Fuel Burnup in Swiss Nuclear Power Reactors and Pro-
visional RIA Licensing Criteria

Fuel elements for the Swiss nuclear power reactors (three PWRs and two BWRs) are
supplied by four different fuel manufacturers. The licensing of a new fuel element type con-
siders safety aspects of normal operation and design-basis accidents. Based on submitted
assembly and fuel rod design reports, allowable burnup limits are determined and licensed
on a fuel-type specific basis. The corresponding maximum assembly-average, rod-average
and pellet-average burnups licensed for each of the five Swiss nuclear power reactors are
shown in Table I.

<table>
<thead>
<tr>
<th>Reactor</th>
<th>Beznau I (PWR)</th>
<th>Beznau II (PWR)</th>
<th>Gösgen (PWR)</th>
<th>Leibstadt (BWR/6)</th>
<th>Mühleberg (BWR/4)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fuel supplier</td>
<td>Westinghouse</td>
<td>Siemens</td>
<td>Siemens</td>
<td>ABB</td>
<td>General Electric</td>
</tr>
<tr>
<td>Assembly-average burnup</td>
<td>48</td>
<td>50</td>
<td>60</td>
<td>51</td>
<td>50</td>
</tr>
<tr>
<td>Rod-average burnup</td>
<td>60</td>
<td>55</td>
<td>70</td>
<td>n.c.</td>
<td>n.c.</td>
</tr>
<tr>
<td>Pellet-average burnup</td>
<td>n.c.</td>
<td>59</td>
<td>76</td>
<td>65</td>
<td>70</td>
</tr>
</tbody>
</table>

n.c.: not considered in the nuclear design and evaluation of reload cores

Table I: Licensed Burnup Limits in Swiss Nuclear Power Reactors (MWd/kgU).

Compared to the licensed burnup limits imposed in other countries (e.g. 47 MWd/kgU for
the assembly-average burnup in France and 60 MWd/kgU for the rod-average burnup
in the USA) higher values (up to 60 MWd/kgU for the assembly-average burnup and up to
70 MWd/kgU for the rod-average burnup) are applicable in some Swiss reactors. Actually,
assembly-average burnups up to 56 MWd/kgU, rod-average burnups up to 58 MWd/kgU
and pellet-average burnups up to 66 MWd/kgU have been attained in Switzerland by the summer of 1995; single test fuel rods have been irradiated up to a rod-average burnup of 82 MWd/kgU.

As in the USA, until 1994 HSK prescribed a radially-averaged pellet enthalpy limit of 280 cal/gUO$_2$ for analysis of RIAs such as the rod ejection accident in a pressurized water reactor (PWR) or the rod drop accident in a boiling water reactor (BWR). This limit was imposed to prevent a widespread dispersal of fuel with a high enthalpy content into the coolant, thus avoiding severe core damage and the generation of severe pressure pulses in the primary system [1], [2]. For the radiological consequence analysis a cladding failure had to be assumed if the radially-averaged pellet enthalpy was greater than 170 cal/gUO$_2$ at any location in a rod [2].

Based on the recent RIA test results HSK proposed the following provisional licensing criteria for RIAs (see section 1):

1. Severe core damage or severe pressure pulses in the reactor coolant system can be excluded,
   (a) if a cladding failure (see point 2 below) does not occur for all rods in the core, or
   (b) if fuel melting in the entire pellet, including the fuel rim region, is avoided.

2. For the radiological consequence analysis a cladding failure has to be assumed if the radially-averaged pellet enthalpy increase during the RIA (AE given in cal/gUO$_2$) as a function of pellet-average burnup B (given in MWd/kgU) is larger than the following provisional criterion:

$$\Delta E = 125 - 1.6 B.$$  

The following comments apply to these increased provisional safety requirements:

- Reactors must be designed such that fuel melting does not occur during anticipated operational occurrences. For accidents such as rod ejection or rod drop, fuel melting is permitted as long as severe core damage can be excluded. For criterion 1. above, it is assumed that avoiding fuel melting during RIAs prevents a widespread dispersal of fuel with a high enthalpy content into the coolant, and thus excludes severe core damage.

- The equation for the radially-averaged pellet enthalpy increase given above bounds most of the enthalpy values for which cladding failures were observed in RIA tests (Fig. 1). Since the RIA test results are usually presented in terms of total radially-averaged pellet enthalpy, the provisional fuel failure criterion shown in Fig. 1 represents total radially-averaged pellet enthalpies. This quantity is assumed to be 15 cal/gUO$_2$ larger than the radially-averaged pellet enthalpy increase.
Previously, RIAs such as rod ejection or rod drop were evaluated by cycle-independent reference analyses which remained valid for reload cores whose relevant safety parameters (e.g. control rod reactivity worth and delayed neutron fraction) are bounded by the reference analysis. The provisional licensing criteria for RIAs proposed by HSK should serve as a basis for the requested cycle-specific RIA analyses. Therefore, in its letter of November 1994 HSK stated that other criteria fulfilling the RIA design goals discussed above would also be acceptable.

Only a small number of RIA test data with high burnup fuel is available. In addition, based on performed RIA tests and analysis it appears that, besides pellet-average burnup, parameters such as the broadness of the power pulse during the RIA or cladding oxidation and hence fuel rod power history may influence the fuel rod behaviour. Therefore, the licensing criteria for RIAs proposed by HSK were designated as provisional, until an international consensus on more appropriate criteria is reached.
3 Methods and Assumptions

3.1 Calculational Tools and Cross-Section Data

The main tool for the RDA analysis was RAMONA-3B [4], a transient code with a 3D neutronics (1 1/2 groups), multichannel core hydraulics and a fuel heat transfer model. The nuclear steam supply system and the reactor protection system of the plant have been modelled. Albedo parameters are used to represent boundary conditions at the core periphery. RAMONA is suitable for a wide range of plant transients, and has been successfully applied for RDA transients [5], [6].

The static nodal reactor analysis code SIMULATE [7] was used to validate the RAMONA core model and the cross-section data. SIMULATE employs high order spatial flux representation, an advanced fuel assembly homogenization model and an explicit treatment of the reflector. The code has been benchmarked for PWRs and BWRs [8].

The cross-section data for both RAMONA and SIMULATE were generated with CASMO-3 [9], a transport theory lattice physics code which has been benchmarked against measured critical experiments and higher-order calculations [10]. Cross-section data for all 30 lattice types of the reactor analyzed were generated for hot conditions and equilibrium xenon at nominal power. They are functions of exposure, exposure-weighted void, moderator density, fuel temperature and control rod presence. As discussed in section 3.3, the RDAs analyzed were initiated from xenon-free conditions; xenon contributions to the nuclear data were corrected out using xenon coefficients in RAMONA, and microscopic xenon cross-sections in SIMULATE. Delayed neutron parameters (delayed neutron fractions, decay constants and average neutron velocities) were also generated in the CASMO calculations, and fits of these data as a function of exposure and void history were produced for the RAMONA calculations.

3.2 Models and Validation

The 3138 MWth Leibstadt BWR/6 plant has a core of 648 fuel assemblies; the core of cycle 9 (1992/93), which was analyzed in this study, contains 440 GE and 208 SVEA-96 assemblies. The control rods of this plant are shorter than the active fuel length; thus there is an "uncontrolled" volume of about 16 cm height at the top of the core.

A full core RAMONA model was developed with one hydraulic channel per neutronic channel and 25 axial nodes. A series of static calculations were performed to qualify this RAMONA model against experiments and SIMULATE results. The full core SIMULATE model employed was developed for an earlier project, in which it was validated by core follow calculations and TIP comparisons for cycles 1 to 9 [11].

The plant utility delivered the burnup and void history distributions for beginning and end of cycle 9 (BOC9 and EOC9), which were used as input for RAMONA and SIMULATE. In addition it provided 13 control rod patterns for critical core configurations at BOC which were determined by experiments. Recalculating these critical rod patterns with RAMONA and SIMULATE gave a satisfactory agreement in $k_{eff}$, although the cross-sections had to be extrapolated from hot to cold conditions.
3.3 Initial and Boundary Conditions

The RDA is the result of a postulated event in which a high worth control rod drops from the fully inserted position in the core. The rod becomes decoupled from its drive mechanism and is assumed to be stuck in place. After the mechanism has been withdrawn, the control rod becomes unstuck and falls until it reaches the control rod drive position. This results in a large reactivity addition and a power excursion.

Protection against the RDA is provided by the constraints of the banked position withdrawal sequence (BPWS) [12], [13], which restricts the possible control rod patterns and the "dropping distance", and thus limits the worth of the dropped rod. The BPWS is enforced by the rod pattern control system (RPCS) in the plant. This system limits the possible dropping distance to as low as 4 "notches" (a notch is 1/4 ft = 7.62 cm) for some positions of the associated rod group [13]. The initial control rod configuration for this analysis is either the "black and white" control rod pattern (control rod groups 1 to 4 withdrawn) according to withdrawal sequence A [13], or with rod groups 1 to 5 withdrawn (Fig. 2). For these initial configurations $k_{eff}$ of the reactor is close to 1.

![Fig. 2: Numbering of control rods and control rod groups for the Leibstadt BWR/6 core.](image-url)
For this analysis EOC conditions were chosen, to obtain pellet-average burnups greater than 50 MWd/kgU. The analysis starts with the initial conditions for a start-up at hot zero power (HZP) conditions used in the RDA analysis of the plant safety analysis report [12]:

- Initial condition: HZP (3kW)
- Initial flow rate: 3456 kg/s
- Inlet subcooling: 2.5 K
- System pressure: 72.3 bar
- Xenon distribution: No Xe
- Rod drop velocity: 1.5 m/s

3.4 Beyond-Design-Basis Case Analyzed

As discussed in section 4, for the EOC9 configuration no control rod worths above $1 could be identified when proper credit was given to the RPCS, and the corresponding enthalpy increases for design-basis RDAs were found to be negligible. In order to investigate the appropriate means of analyzing RIAs considering the increased safety requirements, it was deemed important to analyze cases for which significant enthalpy increase results. An obvious way to accomplish this is by considering super-prompt-critical cases. For this reason, the dropping distance restrictions of the BPWS were neglected, and it was assumed that control rods dropped their total insertable length, 48 notches, with the associated group fully inserted. It is important to note that, because of this assumption, the transient calculations discussed in the remainder of this article are for beyond-design-basis conditions.

3.5 Power and Burnup Peaking Factors

Local power peaking factors as derived by the assembly code CASMO are factored into the nodal enthalpies calculated by RAMONA to obtain the radially-averaged (over the pin) maximum enthalpy increase (henceforth termed "enthalpy increase") in the nodes.

Peaking of the local burnup distribution within the assembly is not accounted for in RAMONA. Hence a local burnup peaking factor is applied to the nodal burnup values during post-processing, to obtain the maximum pellet-average burnup (henceforth called "pellet-average burnup") in the nodes. Since detailed information about the location of burnup and enthalpy peaks was not available in this analysis, the conservative assumption was made that these peaks occur in the same pin (which is not in general to be expected).

The peaking factors describing the burnup distribution tend to diminish with increasing burnup. An approximate fuel-type independent correction was made with the expression:

\[
\text{local peaking factor for burnup} = 1.23 - 0.0024 \times (\text{nodal burnup}),
\]

where the nodal burnup is in MWd/kgU.

Some code modifications were implemented in RAMONA to allow for detailed edits of the nodal enthalpies and the nodal burnup.
4 Static Calculations

A series of steady-state RAMONA calculations were performed to determine static rod worth. Due to the quarter core rotational symmetry, only one quarter of the core needed to be considered. Most of the possible rod drops considering the BPWS limitations were calculated, and some of the results are shown in the first part of Table II for "Design-basis conditions".

<table>
<thead>
<tr>
<th>Case</th>
<th>Code</th>
<th>Withdrawn groups</th>
<th>Control rod group</th>
<th>Banked at ([n])</th>
<th>Control rod</th>
<th>Drops from ([a]) to ([b])</th>
<th>(\Delta k)</th>
<th>(\Delta \rho)</th>
</tr>
</thead>
<tbody>
<tr>
<td>D1</td>
<td>RAM</td>
<td>1 to 4</td>
<td>7</td>
<td>0</td>
<td>4243</td>
<td>0 (\rightarrow) 4</td>
<td>0.00273</td>
<td>0.50</td>
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<td>D2</td>
<td>RAM</td>
<td>1 to 4</td>
<td>7</td>
<td>4</td>
<td>4243</td>
<td>0 (\rightarrow) 8</td>
<td>0.00214</td>
<td>0.39</td>
</tr>
<tr>
<td></td>
<td>SIM</td>
<td>1 to 4</td>
<td>7</td>
<td>4</td>
<td>4243</td>
<td>0 (\rightarrow) 8</td>
<td>0.00278</td>
<td>0.51</td>
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<td>D3</td>
<td>RAM</td>
<td>1 to 4</td>
<td>8</td>
<td>4</td>
<td>4235</td>
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<td>0.45</td>
</tr>
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<td></td>
<td>SIM</td>
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<td>8</td>
<td>4</td>
<td>4235</td>
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<td>RAM</td>
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<td>4</td>
<td>3055</td>
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<td></td>
<td>SIM</td>
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<td>10</td>
<td>4</td>
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<td>RAM</td>
<td>1 to 4</td>
<td>7</td>
<td>8</td>
<td>4243</td>
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<tr>
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<td>RAM</td>
<td>1 to 4</td>
<td>7</td>
<td>12</td>
<td>4243</td>
<td>0 (\rightarrow) 48</td>
<td>0.00091</td>
<td>0.16</td>
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</tbody>
</table>

Beyond-design-basis conditions:

<table>
<thead>
<tr>
<th>Case</th>
<th>Code</th>
<th>Withdrawn groups</th>
<th>Control rod group</th>
<th>Banked at ([n])</th>
<th>Control rod</th>
<th>Drops from ([a]) to ([b])</th>
<th>(\Delta k)</th>
<th>(\Delta \rho)</th>
</tr>
</thead>
<tbody>
<tr>
<td>B1</td>
<td>RAM</td>
<td>1 to 4</td>
<td>7</td>
<td>0</td>
<td>4243</td>
<td>0 (\rightarrow) 48</td>
<td>0.01219</td>
<td>2.21</td>
</tr>
<tr>
<td>B2</td>
<td>RAM</td>
<td>1 to 5</td>
<td>7</td>
<td>0</td>
<td>3451</td>
<td>0 (\rightarrow) 48</td>
<td>0.01146</td>
<td>2.08</td>
</tr>
<tr>
<td>B3</td>
<td>RAM</td>
<td>1 to 5</td>
<td>10</td>
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<td>3055</td>
<td>0 (\rightarrow) 48</td>
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<td></td>
<td>SIM</td>
<td>1 to 5</td>
<td>10</td>
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<td>3055</td>
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<td>RAM</td>
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<td>3847</td>
<td>0 (\rightarrow) 48</td>
<td>0.01157</td>
<td>2.10</td>
</tr>
</tbody>
</table>

RAM: RAMONA
SIM: SIMULATE

\([a]\): notch position (1 notch = 0.25ft = 0.762cm)
\(\beta_{eff}\) for EOC 9 = 0.0054

Table II: Static Rod Worths for the Leibstadt BWR/6 Reactor at HZP and EOC9.

Table II contains all the necessary information to describe a rod drop. The rod which drops is specified in the column "Control rod" and its core position can be seen in Fig. 2 (in this article control rods and assemblies are designated cr, where c is the column number and r is the row number indicated in Fig. 2). The dropped control rod belongs to the "Control rod group" given in Table II which is "Banked at" the specified number of withdrawn notches. The stuck rod "Drops from \([a]\) to \([b]\)", where a always specifies...
the fully inserted position (0 notches withdrawn) and b gives the position to which the rod drops (number of withdrawn notches). The position of the other control rod groups can be seen in the column "Withdrawn groups". The control rod groups specified here are fully withdrawn, while all other groups except the group of the dropped rod are fully inserted. Finally the calculated rod worths are given in the last two columns. \( \Delta k \) is the difference in \( k_{\text{eff}} \) between control rod position a and b, while \( \Delta \rho \) gives the corresponding reactivity in \$\ for a delayed neutron fraction \( \beta_{\text{eff}} \) of 0.0054 at EOC9.

The results shown in the first part of Table II (cases D1 - D6) are typical for design-basis conditions. All calculated rod worths were well below a reactivity of \$1. In addition some SIMULATE results are given, from runs made to check the RAMONA calculations. The general agreement between between RAMONA and SIMULATE rod worth lies in the range of 20%.

The second part of Table II (cases B1 - B4) shows the static rod worths as calculated for the beyond-design-basis conditions. All these cases are super-prompt-critical, with rod worths well above \$1. The worth of rod 3055, which was the only one of these cases checked by SIMULATE, shows good agreement with the RAMONA result.

5 Transient Calculations

5.1 Overview of Results

The beyond-design-basis RDA cases B1 - B4 of Table II have been considered in the transient calculations, with static control rod worths calculated by RAMONA ranging between \$2.1 and \$3.6. The core analyzed has a low-leakage loading pattern with high-burnup fuel assemblies at the core periphery. At BOC, the 20% fresh assemblies are put into positions between assemblies with more burnup. Therefore, assemblies with relatively elevated burnup are found throughout the core. In addition, the control rod with the highest rod worth for the conditions at hot zero power with five groups of control rods removed, case B3 of Table II, is located close to the core periphery.

Since the provisional licensing criteria proposed by the HSK are dependent on burnup, a careful evaluation of the RIA results with respect to burnup is mandatory. Therefore, all \((648 \times 25 = 16200)\) nodes representing the fuel in the computational model of the core are evaluated.

It should be noted that the results reported herein are for basically best-estimate calculations, and a systematic uncertainty analysis of the results was not performed. Thus uncertainties are not indicated for the plotted results, and the following discussion is based on the best-estimate values. Based on various reports concerning the uncertainty of the Doppler coefficient, \[14\], \[15\] and the fact that it varies with the fuel temperature \( T_F \) approximately as \( T_F^{-1/2} \), a "positive-side" uncertainty for the peak enthalpy values of the order of +25% does not seem unreasonable. Another source of uncertainty is the use in RAMONA of fuel material properties which are not burnup-dependent. Obviously the enthalpy uncertainty should be considered in any safety analysis for a specific plant.
The following discussion will concentrate on the analysis of the case with the highest worth of the dropped control rod for the cases in Table II. For this RDA case, B3, Fig. 3 and 4 show the relative core power and peak total enthalpy, respectively. The control rod starts dropping at 0.1 s and is fully out of the core at 2.5 s. The resulting power excursion reaches its maximum around 0.5 s (Fig. 3), at which time the control rod has only moved about 15% of the total dropping distance of 48 notches.

Fig. 3: Case B3 of Table II: Relative core power

Fig. 4: Case B3 of Table II: Hot spot total enthalpy.

Fig. 5 shows the enthalpy increase for each node, plotted against pellet-average burnup at the transient time when the maximum enthalpy in the core of 121.6 cal/g was reached. It is evident that even the best-estimate values for several assemblies exceed the proposed licensing criterion. Also, one assembly with burnup above 40 MWd/kgU received enough thermal loading to exceed the criterion for this beyond-design-basis accident, even though its enthalpy value is well below the core maximum.

To facilitate the interpretation, the large number of nodes can be subdivided. An evident approach is to separate out the "controlled" nodes. For the same conditions, controlled nodes (Fig. 6) carry a low thermal loading compared to the uncontrolled nodes, and a maximum enthalpy increase of 49 cal/g (at around 20 MWd/kgU) is found for the former. None of the enthalpy increase values exceed the provisional licensing criterion.

Likewise, Fig. 7 shows the enthalpy increase for the "uncontrolled" nodes of case B3. The enthalpy increase of several assemblies exceed the provisional licensing criterion. Quite a few nodes are very close to exceeding the criterion. If an appropriate enthalpy adder for uncertainty would be used (as discussed above), more bundles would exceed the criterion. For the other cases analyzed (B1, B2 and B4 of Table II), none of the best-estimate values for the uncontrolled or controlled nodes exceeded the criterion.
Fig. 5: Case B3 of Table II: Radially-averaged peak fuel enthalpy increase during the transient versus pellet-average burnup for all nodes in the core.

Fig. 6: Case B3 of Table II: Radially-averaged peak fuel enthalpy increase during the transient versus pellet-average burnup for the controlled nodes.
For case B3, the four assemblies surrounding the dropped control rod 3055 (row 1) carry a high thermal load, as shown in Fig. 8, but not the highest in the core. The peak enthalpies of the four assemblies do not vary strongly and the enthalpy values of the two assemblies with the elevated burnup exceed the criterion. The peak enthalpy is found near the core top for all of these assemblies, indicating a very "top-peaked" axial power distribution.

The 12 assemblies surrounding row 1 are designated as row 2. Fig. 9 shows the enthalpy increase for the four row 2 assemblies nearest to the centre of the core. The two assemblies with low burnup experience the highest enthalpy values. For the two assemblies with higher burnup (3152 and 3352) the provisional licensing criterion is not exceeded for the nodes with the assembly maximum enthalpy increase, but is for the adjacent nodes. This indicates that the evaluation of just the assembly peak values for the fuel enthalpy increase is not adequate for the evaluation of RDA results (for super-prompt-critical transients) against the burnup-dependent provisional licensing criterion.

It is interesting to note that for none of the remaining 8 assemblies in row 2 have enthalpy increase values in excess of the licensing criterion been found. The four assemblies of row 2 which are located closest to the core periphery have burnups comparable to the ones of the two assemblies 3152 and 3352. However, their enthalpy increase values are only around 50% of the values for the assemblies 3152 and 3352. This lower thermal load is beneficial for the highly burned assemblies at the periphery of the low-leakage core.
Fig. 8: Case B3 of Table II: Radially-averaged peak fuel enthalpy increase during the transient versus pellet-average burnup for the row 1 assemblies.

Fig. 9: Case B3 of Table II: Radially-averaged peak fuel enthalpy increase during the transient versus pellet-average burnup for the four row 2 assemblies nearest to the centre of the core.
A comparison of the plots for the two adjacent assemblies 2752 and 2952 on Fig. 9 suggests that strong radial flux gradients must have prevailed during the power excursion. This points to the necessity of a powerful transient pin-power-reconstruction module for best-estimate analysis of such transients. For this study the local power peaking factor derived from static assembly calculations with reflective boundary conditions was applied to the nodal enthalpy values.

Fig. 10 shows the enthalpy increase for the six row 3 assemblies nearest to the centre of the core. For the assembly with the highest burnup of this group, the enthalpy increase values exceed the criterion at pellet-averaged burnups in the range of 42 to 50 MWd/kgU. Thus it may be necessary to consider elements at relatively large distances from the dropped rod.

5.2 Dependence of Fuel Enthalpy on Burnup

The power distribution at steady-state is influenced by the burnup distribution; highly burned fuel sees relatively low power and vice versa. It is therefore of special interest to investigate how the enthalpy increase depends on burnup. For this analysis, the assemblies within the rows 1 - 3 for the cases B1 - B4 of Table II have been considered. Based on the above discussions, only the nodes at the axial location of the maximum (algebraic) difference \(^1\) between the nodal enthalpy increase and the provisional licensing criterion were scanned. Furthermore, only the uncontrolled nodes were selected.

Fig. 11 shows the radially-averaged fuel enthalpy of the nodes identified as discussed above. An interesting feature is found; for the case with the highest worth of the dropped control rod, the dependence of the enthalpy increase on burnup is stronger than for the others. For the case B3 the least-square-fit line is almost parallel to the line representing the provisional licensing criterion. This is a favourable situation; for the more severe case (B3) relatively less enthalpy is stored in the fuel at high burnup nodes while for cases with a control rod of lower worth (B1, B2, B4) the enthalpy is distributed more evenly as a function of burnup, but at a much lower level.

5.3 Dependence of Fuel Enthalpy on Distance from the Dropped Rod

For the four cases B1 - B4 of Table II the dependence of the radially-averaged enthalpy increase on the distance from the dropped rod was determined. The nodes were selected in the same way as for Fig. 11. The enthalpy increase dependence on the distance from the dropped rod was the strongest for the case with the highest rod worth.

\(^1\)That is, if the value is above the criterion, the absolute difference is the maximum for that assembly, whereas for values below the criterion the absolute difference is the assembly minimum.
Fig. 10: Case B3 of Table II: Radially-averaged peak fuel enthalpy increase during the transient versus pellet-average burnup for the six row 3 assemblies nearest to the centre of the core.

Fig. 11: Cases B1 - B4 of Table II: Least-square fit for the uncontrolled nodes of the row 1 - 3 assemblies with maximum enthalpy difference to the provisional licensing criterion.
6 Summary and Conclusions

Based on the recent high burnup fuel RIA test results, the Swiss Federal Nuclear Safety Inspectorate requested plant- and cycle-specific analyses of the rod ejection or the rod drop accident for the Swiss nuclear power reactors. As a basis for these analyses provisional burnup-dependent licensing criteria for reactivity initiated accidents were proposed. In the present study some aspects of the impact of the proposed criteria on RDA analysis for boiling water reactors were investigated.

The calculations were performed for the Leibstadt BWR/6 plant at hot zero power and end of cycle 9 conditions using the RAMONA-3 code. To be able to compare the calculational results with the burnup-dependent fuel failure criterion, code modifications had to be implemented in RAMONA-3 to allow for detailed edits of nodal enthalpies and burnups, and the radial burnup distribution within an assembly had to be approximated by a simplified procedure.

A systematic uncertainty analysis was not performed, and in the following statements, conclusions regarding cases which exceed the cladding failure criterion are for basically best-estimate results.

For design-basis RDAs, in which the control rod pattern and dropping distance restrictions enforced by the Rod Pattern Control System (RPCS) are effective, static control rod worths up to $\Delta k = 0.0027$ were calculated, and the corresponding fuel enthalpy increases during these RDAs were found to be negligible.

For beyond-design-basis RDAs, in which the dropping distance restrictions of the RPCS were neglected and the rods were dropped their total insertable length, three RDA cases with static control rod worths around $\Delta k = 0.012$ and one case with $\Delta k = 0.020$ were investigated. Only in the latter case did the calculated enthalpy values exceed the proposed provisional fuel failure criterion in some assemblies.

Controlled nodes carry a low thermal load. None of the enthalpy increase values for such nodes exceeded the provisional licensing criterion.

It was demonstrated that the provisional licensing criterion may be exceeded for nodes other than that which has the maximum assembly enthalpy increase, even though for the latter the criterion is not exceeded. This indicates that the evaluation of all nodes can be necessary.

It was found that the criterion was exceeded for assemblies several rows away from the dropped rod. Thus it may be necessary to consider elements at relatively large distances from the dropped rod.

The dependence of the enthalpy increase on burnup was stronger for the case with the highest worth of the dropped control rod than for the other cases. This is a favourable situation; for the more severe case relatively less enthalpy was stored at high burnup nodes.
It should be pointed out that the "trends" described above were derived from the analysis of a limited number of cases at the end of a single cycle. Analysis of more cases and of other cycles are necessary for deriving general trends. In addition, an identification of the relevant physical phenomena is deemed necessary.

Acknowledgments

The authors would like to thank W. van Doesburg, R. Lundmark, C. G. Wiktor and H.-U. Zwicky of the Leibstadt nuclear power plant for discussions and for providing plant data.

References


ANALYSES OF ROD DROP ACCIDENTS USING A THREE DIMENSIONAL TRANSIENT CODE FOR REACTIVITY-INITIATED EVENTS OF BOILING WATER REACTORS

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1. Introduction

A reactivity insertion event is defined as a rapid transient of reactor power caused by a reactivity insertion of over $1.0$. For a boiling water reactor (BWR), one of the design basis accidents is a postulated drop of a control rod from the critical core, which is called a rod drop accident (RDA).

Traditionally, a point kinetics model has been used to analyze this event, together with a simple feedback model that neglects moderator density reactivity feedback. However, moderator density reactivity feedback plays an important role in suppressing the maximum fuel enthalpy which is one of the most important parameters for determining the fuel integrity following a reactivity insertion event.

More detailed treatments of the core conditions are necessary in order to obtain accurate evaluation results, and the detailed power distribution and fuel burnup distribution must be considered. Recent advances in modern computer technology have enabled departure from the use of approximate and simple methods.

The ARIES code is a three-dimensional dynamic code, which can handle a multi-node treatment in the axial and radial directions for detailed evaluation of RDAs in BWR's. This code has been verified with a three-dimensional benchmark problem posed by the Nuclear Energy Agency Committee on Research Physics / Committee on the Safety of Nuclear Installations (NEACRP / CSNI) and against experimental data from reactivity accident tests performed with the SPERT III E-core (Ref. 1). ARIES has a three-dimensional neutron diffusion model and a multi channel one-dimensional (axial direction) thermal-hydraulic model in order to take into account the coupling effect of neutronics and thermal-hydraulics (Ref. 2). In contrast, a traditional analysis codes which have been used for licensing analyses of BWR's do not take into account this coupling effect and only the
Doppler effect is taken into account as a reactivity feedback mechanism, where point kinetics model combined with two dimensional (R-Z) spatial neutronics is employed.

Recent results of reactivity insertion experiments indicate that high burnup fuel might fail under conditions with fuel enthalpy lower than previously considered. Although the number of experimental data is very limited, the most recent data in France indicate that fuel rod cladding failure may occur at an enthalpy of 30 cal/g for high burnup fuel (with more than 60 GWD/MTU). Such a decrease of the fuel failure threshold value may affect the fuel safety following an RDA.

Considering that high burnup fuel bundles occupy only a small portion of the core in real operating plant, the consequences of an RDA may reasonably be expected not to change much despite the above new findings. Furthermore, since high burnup fuel does not have enough reactivity to reach a high fuel enthalpy state, it is important to reassess the RDA events using ARIES code which can estimate enthalpy distribution in the core.

This paper presents the evaluation results of RDAs using the three-dimensional ARIES code and a comparison with the results of traditional licensing analysis code which has simple models. It also presents the results of the estimated failed fuel rods number assuming lower fuel failure thresholds for high burnup fuel rods. The evaluations were performed for a typical 3293-MW (thermal) BWR, with a core composed of a type of GE9 fuel bundles whose assembly average discharge burnup was approximately 40,000MWD/MTU.

2. The models of ARIES code

The simulation code ARIES features a three-dimensional multinode treatment of the fuel rods with equations applicable for the rapid transient. In the neutronics phase, ARIES solves a time-dependent, modified one energy group, coarse-mesh three-dimensional neutron diffusion equation and equations for a maximum of six groups of delayed neutron precursor concentrations. In the thermal-hydraulic phase, ARIES solves the time-dependent, nonhomogeneous, non equilibrium one-dimensional thermal-hydraulic equations, which treat parallel coolant flow channels. Fuel cladding heat transfer equations are used for each channel. Each channel is associated thermal-hydraulically with channel inlet flow distribution to balance the pressure drops across each channel. The thermal-hydraulic model takes into account subcooled boiling. This has a non negligible effect on peak fuel enthalpy if RDA occurs under large core inlet coolant subcooling conditions. The reactivity perturbation model considers control rod removal. This modeling is made feasible by the three-dimensional model.

ARIES also contains the thermal-hydraulic model for the dome to evaluate reactor pressure. In a typical RDA, this pressure changes considerably due to rapid generation of steam voids. The recirculation system model is also included to evaluate core inlet flow. This flow also fluctuates...
depending on the perturbation of core pressure drops due to void fraction changes. Core inlet flow is distributed to individual channels in such a way that the individual channel pressure drops are equalized. This is to prevent the local pressure drop perturbations caused by changes in local void fractions.

3. Analysis Conditions

Initial conditions of the core for the rod drop accident using ARIES include core thermal power, core inlet flow, dropped control rod worth, etc. Initial conditions for both the cold start up and the hot standby cases are listed in Table 1.

The core configuration is that of a typical BWR-5 design and the rated thermal power is 3,293MWt. The design of the fuel bundle in the core is a type of GE9 and core average discharge exposure is approximately 40,000MWd/MTU. The distribution of bundle average exposure in the core at the beginning of cycle is displayed in Figure 1 and at the end of cycle in Figure 2, this fuel loading pattern is typical in usual operating plant.

The reactor is assumed to be critical at the start of the event. The dropped control rod worth is conservatively 1.5% Δk and its location is in the center of the core. Since such a high rod worth is impossible in the usual fuel loading pattern as shown in Figure 1 and 2, fuel bundles which have rather high reactivities are concentrated in the center of the core for making the dropped control rod worth of 1.5% Δk (Figure 3).

The moderator density reactivity feedback, which plays an important role in suppressing the maximum fuel enthalpy, is taken into account in this analysis.

The above analysis conditions, except for the treatment of moderator density reactivity feedback, are the same as for the traditional licensing analyses.

Table 1 Initial conditions

<table>
<thead>
<tr>
<th></th>
<th>Cold Start-up Core</th>
<th>Hot Standby Core</th>
</tr>
</thead>
<tbody>
<tr>
<td>Initial core thermal power (MW)</td>
<td>$3.293 \times 10^{-5}$ ($10^{-8}$ rated)</td>
<td>3.293 x $10^{-3}$ (10^-6 rated)</td>
</tr>
<tr>
<td>Core flow (T/h)</td>
<td>$9.7 \times 10^{3}$ (20% rated)</td>
<td>$9.7 \times 10^{3}$ (20% rated)</td>
</tr>
<tr>
<td>Dropped control rod worth (% Δk)</td>
<td>1.5</td>
<td>1.5</td>
</tr>
<tr>
<td>Dropped control rod speed (cm/s)</td>
<td>95</td>
<td>95</td>
</tr>
<tr>
<td>Initial fuel enthalpy (cal/g • UO₂)</td>
<td>2</td>
<td>18</td>
</tr>
<tr>
<td>Core average burnup(GWd/t)</td>
<td>17.0(BOC) / 26.7(EOC)</td>
<td></td>
</tr>
<tr>
<td>Highest bundle burnup(GWd/t)</td>
<td>36.2(BOC) / 43.6(EOC)</td>
<td></td>
</tr>
</tbody>
</table>

3 5 5
4. Analytical Results

4.1 Base case analyses

Figure 4 indicates the trend in core thermal power for a cold start-up case at the beginning of cycle (BOC). Figure 5 indicates the trend of total reactivity, which is the sum of dropped rod worth, and Doppler and moderator feedback reactivities of the same case. SCRAM reactivity insertion did not occur in time to suppress the RDA event so its effect could be neglected.

The power increased rapidly right after inserted reactivity became larger than $1.0$ and was suppressed by Doppler and moderator density reactivity feedback in a short time. Since in the cold start-up case condition there is a large core inlet coolant subcooling, the moderator density reactivity feedback was delayed due to the time lag of thermal effects and so had a comparatively small role in suppressing the power.

Figure 6 and Figure 7 show the trend of the same parameters for the hot standby case at BOC. In this case, because of the small core inlet subcooling, the moderator density reactivity feedback effects were observed earlier than in the cold start-up case by the void generation. Results indicated that moderator density reactivity feedback acted as early as the Doppler feedback did for the hot standby case.

The difference between the cold start-up and the hot standby cases is important when the prompt enthalpy increase is focused on. A prompt enthalpy increase can be considered to occur adiabatically. Figure 8 and Figure 9 show the enthalpy changes for the cold start-up and the hot standby cases.

Table 2 summarizes the analysis results of the ARIES code and those of the conservative code which has been used for licensing analysis.

In the cold start-up case, due to the delay of moderator density reactivity feedback which has been mentioned, there is little difference in the estimation of prompt enthalpy increase between the ARIES analysis, which takes into account moderator density reactivity feedback, and the conservative code analysis which neglects that.

On the contrary, in the hot standby case, since the moderator density reactivity feedback plays an important role for power suppression, prompt enthalpy increase is much smaller in the ARIES results than in the conservative analysis results.
The fuel enthalpy became highest in the run-out duration which followed the prompt power increase. The maximum fuel enthalpy calculated by ARIES and the conservative code are listed in Table 3. Since there was a large amount of moderator density reactivity feedback in the run-out duration even in the cold start-up case, the value from ARIES is far smaller than that of the conservative code. This effect is more dramatic in the hot standby case than the cold start-up case.

In current Japanese licensing evaluations, the fuel rod integrity is inferred from the maximum fuel enthalpy and the threshold enthalpy is defined by the "Guideline for the Evaluation of Reactivity Initiated Events in Light Water Reactors". Figure 10 shows the threshold enthalpy of fuel integrity in the above guideline for a reactivity insertion event, and the threshold of 92 cal/g • UO₂ has been used for counting the number of failed fuel rods following an RDA. 92 cal/g • UO₂ was derived from the threshold enthalpy of a fuel rod experiencing an internal-external pressure difference of 30 kg/cm².

This evaluation was more conservative than the evaluation using a threshold of 85 cal/g • UO₂ as the adiabatic prompt enthalpy increase, which came from the experiment of SPERT-CDC using irradiated fuel rods, because 92 cal/g • UO₂ maximum fuel enthalpy corresponds approximately to 60 cal/g • UO₂ adiabatic fuel enthalpy in a traditional RDA analysis. Both thresholds should be used in
ARIES evaluations, because large moderator density reactivity feedback may suppress run-out duration enthalpy quickly.

The numbers of failed fuel rods from the ARIES code analysis are listed in Table 4, compared with the number from the licensing evaluation which used a conservative code without moderator density reactivity feedback. In the ARIES evaluation, the number of failed fuel rods decreased drastically compared to the conservative analysis using the same threshold, because moderator density reactivity feedback greatly decreased the maximum fuel enthalpy. There was no failure of fuel rods in the hot standby case because the moderator density reactivity feedback had a large effect in this case and the maximum fuel enthalpy did not exceed either of the failure thresholds.

<table>
<thead>
<tr>
<th>Number of failed fuel rods</th>
<th>Cold start-up case</th>
<th>Hot standby case</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>BOC</td>
<td>EOC</td>
</tr>
<tr>
<td>Conservative code</td>
<td>3,725</td>
<td>1,133</td>
</tr>
<tr>
<td>ARIES code</td>
<td>200</td>
<td>200</td>
</tr>
</tbody>
</table>

### Table 4: Number of failed fuel rods between conservative code and ARIES code

#### 4.2 Sensitivity Analyses

**1. Dropped control rod worth**

Another parameter which has a strong effect on RDA is the dropped control rod worth. The highest worth of a control rod in a realistic fuel loading pattern for a BWR core is less than 1.0% Δk, as shown in Figure 11, which indicates the trend of the control rods worth during start-up of a typical BWR.

In order to quantify the conservatism of RDA analyses, the case of a 1.0% Δk dropped control rod worth was carried out for the condition of the cold start-up at EOC. The analysis results are listed in Table 5, along with the base case results. Maximum fuel enthalpy was limited to 75 cal/g · UO₂ due to the decrease of inserted reactivity, so there was no fuel failure due to the decrease of fuel enthalpy. This result indicates a large conservatism in the analysis condition of 1.5% Δk dropped rod worth.

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Table 5 Sensitivity analysis on the dropped control rod worth

<table>
<thead>
<tr>
<th></th>
<th>Base case analysis using 1.5% $\Delta k$</th>
<th>Sensitivity analysis using 1.0% $\Delta k$</th>
</tr>
</thead>
<tbody>
<tr>
<td>Prompt enthalpy increase (cal/g • UO₂)</td>
<td>93</td>
<td>57</td>
</tr>
<tr>
<td>Maximum fuel enthalpy (cal/g • UO₂)</td>
<td>109</td>
<td>75</td>
</tr>
<tr>
<td>Number of failed fuel rods</td>
<td>200</td>
<td>0</td>
</tr>
</tbody>
</table>

(2) Fuel loading pattern

The impact of reactivity insertion by the control rod drop is limited to the local area around the dropped control rod as shown in Figure 12. Another sensitivity analysis was done to find the effect of the loading pattern around the dropped control rod cell. The highest burnup fuel bundle in the core was moved to the cell adjacent to the dropped control rod cell, and the fuel bundles which have rather high reactivities were concentrated adjacent to the dropped control rod cell for keeping the dropped control rod worth of 1.5% $\Delta k$ (Figure 13).

The analysis results are displayed in Table 6, compared with the base case evaluation. The maximum fuel enthalpy is almost the same as in the base case result. On the other hand, the maximum fuel enthalpy of highest burnup fuel was limited to 52 cal/g • UO₂ nevertheless it is located in the cell adjacent to the cell of the dropped control rod. Figure 14 indicates the maximum fuel enthalpy of the fuel bundles which are located beside the dropped control rod cell as a function of peak pellet burnup. There is a tendency of the fuel enthalpy to decrease with burnup under the same condition that these fuel bundles are adjacent to the dropped control rod cell. This result is plausible since the amount of fissile material in a rod becomes less with burnup increase. Thus it is reasonable that the maximum fuel enthalpy decreases with burnup.

Table 6 Sensitivity analysis on the fuel loading pattern

<table>
<thead>
<tr>
<th></th>
<th>Base case analysis</th>
<th>Sensitivity analysis on highest burnup fuel loaded adjacent to dropped control rod cell</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Maximum value in the core</td>
<td>Value on the highest burnup fuel</td>
</tr>
<tr>
<td>Prompt enthalpy increase (cal/g • UO₂)</td>
<td>93</td>
<td>98</td>
</tr>
<tr>
<td>Maximum fuel enthalpy (cal/g • UO₂)</td>
<td>109</td>
<td>111</td>
</tr>
<tr>
<td>Number of failed fuel rods</td>
<td>200</td>
<td>208</td>
</tr>
</tbody>
</table>
(3) Burnup dependence of fuel failure threshold

It has not been shown that the threshold enthalpy of fuel failure always decreases with an increase in rod burnup. To determine whether there is such an impact on the safety evaluation, the number of failed fuel rods were evaluated under the assumption of the failure threshold decreases with burnup. Figure 15 shows the threshold enthalpy for the sensitivity analysis. It assumes the following:

- For burnup less than 40GWd/t:
  Adiabatic fuel enthalpy of 85 cal/g • UO₂ or maximum fuel enthalpy of 92 cal/g • UO₂

- For burnup greater than 40GWd/t:
  Enthalpy increase of 15 cal/g • UO₂

Considering that high burnup fuel bundles occupy only a small portion of the core in a real operating plant, it is expected that the number of failed fuel rods does not change so much by the threshold decrease with burnup. The analysis results are shown in Table 7. The maximum number of failed fuel rods, 764, occurred in the sensitivity analysis case in which the highest burnup fuel is loaded adjacent to the dropped control rod cell. Even so, far fewer fuel rods failed in this case than in the current licensing analyses.

Table 7 The number of failed fuel rods using threshold dependent on burnup

<table>
<thead>
<tr>
<th>Dropped control rod worth</th>
<th>Base case analyses</th>
<th>Sensitivity analysis using dropped control rod worth of 1.0% Δk</th>
<th>Sensitivity analysis by loading highest burnup fuel adjacent to dropped control rod cell</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Cold start-up</td>
<td>Hot standby</td>
<td>Cold start-up</td>
</tr>
<tr>
<td></td>
<td>BOC</td>
<td>EOC</td>
<td>BOC</td>
</tr>
<tr>
<td>Failed fuel number of base case evaluation</td>
<td>1.5% Δk</td>
<td>1.0% Δk</td>
<td>1.5% Δk</td>
</tr>
<tr>
<td>Failed fuel number of evaluation by using the threshold dependent on burnup</td>
<td>200</td>
<td>200</td>
<td>0</td>
</tr>
<tr>
<td></td>
<td>400</td>
<td>680</td>
<td>200</td>
</tr>
</tbody>
</table>
5. Conclusion

Rod drop accident (RDA) is one of the design basis accidents for a BWR. Prior to development of modern computer capabilities, simple models combined with the conservative assumption of no moderator density reactivity feedback were used.

Analysis results using ARIES, which has a three-dimensional neutronics model coupled with a thermal-hydraulic model, indicate that the moderator density reactivity feedback is important to suppress the maximum fuel enthalpy for any set of initial conditions. Maximum fuel enthalpy is used to infer the fuel rod integrity in current licensing analyses, and this feedback effect shows a large conservatism in the estimation of the number of failed fuel rods.

Other sensitivity analyses prove the significant conservatism of dropped rod worth and the upper limit of fuel enthalpy reached in high burnup rods.

The number of failed fuel rods affects the dose evaluation through the release of radioactive inventory from failed fuel rods. The dose of RDA in current licensing evaluation is $1.2 \times 10^{-2}$ mSv which corresponds to the failed fuel rods' number of 4,000, and far below against the criteria of safety evaluation, 5 mSv. Thus, from the view of safety evaluation there is enough margin even if the fuel failure threshold decreases with burnup. Furthermore, there is only a little impact on dose evaluation itself considering the current analyses conservatism, as demonstrated by ARIES analyses.

References
Figure 1  The distribution of bundle average exposure at the beginning of cycle (1/4 core)
Figure 2  The distribution of bundle average exposure at the end of cycle (1/4 core)
Figure 3 Fuel loading pattern for usual operation and ARIES analysis (1/4 core)
Figure 4  Power excursion of cold start-up core RDA at the beginning of cycle
Figure 5  Reactivity components of cold start-up core RDA at the beginning of cycle
Figure 6  Power excursion of hot standby core RDA at the beginning of cycle
Figure 7  Reactivity components of hot standby core RDA at the beginning of cycle
Figure 8  Fuel enthalpy of cold start-up core RDA at the beginning of cycle
Figure 9  Fuel enthalpy of hot standby core RDA at the beginning of cycle
Figure 10  Allowable fuel design limit for reactivity initiated event
Figure 11  Typical trend of control rod worth in actual rod withdrawal sequence
Figure 12 Enthalpy distribution in the core (Cold start-up at the end of cycle)
| 1, 2, 3, 4 or 5 : Number of loaded cycle |

| Dropped control rod worth : 1.5% \( \Delta k \) |

| Fuel loading pattern for cold start-up at EOC |

| Fuel exchanged for worth adjustment |

| Highest burnup fuel |

| Figure 13 Fuel loading pattern for sensitivity analysis |

< Fuel loading pattern of maximum burnup fuels loaded adjacent to the dropped control rod cell >

(Cold start-up at EOC)
Figure 14 Maximum fuel enthalpy in accordance with peak pellet burnup
Adiabatic enthalpy of 85 cal/g \cdot UO_2 or maximum fuel enthalpy of 92 cal/g \cdot UO_2

Enthalpy increase of 15 cal/g \cdot UO_2

Figure 15 Fuel failure threshold dependent on burnup

Fuel enthalpy (cal/g \cdot UO_2)

Peak pellet burnup (GWd/t)
Realistic Evaluation of Reactivity Insertion Accidents in Boiling Water Reactors

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1. INTRODUCTION and SUMMARY

Recent tests of high exposure fuel under simulated reactivity insertion accidents (RIAs) indicate that the energy deposition required for fuel cladding perforation may decrease with increasing exposure (Reference 1). As a result, the U.S. Nuclear Regulatory Commission (NRC) and U.S. nuclear industry have undertaken extensive programs to assess the representativeness of the fuel segments used in the tests, the validity of the tests and the potential implications on the licensing criteria for RIAs.

This paper presents the results of a best-estimate evaluation of the limiting RIA in a Boiling Water Reactor (BWR) – the control rod drop accident (RDA). Using a licensed core design which is expected to represent a more-limiting design than a typical core design, the best-estimate analysis results in the following key conclusions:

1. The calculated peak pellet average enthalpies using best-estimate methodology are about 33% lower than the peak enthalpies calculated with the current adiabatic GE licensing basis methodology. For the best-estimate calculation assuming a full drop of a high-worth, in-sequence control rod the peak enthalpy value for the most highly-reactive fuel in the core is only about 45 cal/g.

2. For exposures beyond gadolinium burnout (typically 10 to 15 GWd/t), the peak pellet average enthalpy in the fuel decreases with increasing bundle average exposures. In the best-estimate case presented, the peak pellet average enthalpy is less than 20 cal/g at a bundle average exposure of 40 GWd/t. (NOTE: Exposures are reported per metric ton (t) of uranium.)

3. The RDA is a localized event. The channel power responses outside the four-by-four array of bundles around the dropped rod decrease rapidly as the distance from the dropped rod increases. Three bundles away from the dropped rod the power pulses are less than 20% of what they are in the bundles adjacent to the dropped rod.

4. The increased energy deposition associated with the buildup of plutonium in the peripheral region of the fuel pellet does not significantly impact the calculated maximum value of pellet average enthalpy.

The implications of the CABRI RIA test results are evaluated and discussed in terms of potential new high exposure failure mechanisms and their relevance and applicability to BWR Anticipated Operational Occurrences (AOOs). The fundamental differences between the licensing basis BWR RDA and the most limiting AOO are also evaluated and discussed especially in terms of how these differences may impact
the applicability of any postulated new fuel failure mechanisms. The key conclusions obtained from these assessments are:

1. The low enthalpy failure for CABRI test Na1 does not support the existence of a new high exposure failure mechanism for BWR fuel. The more likely cause of the low enthalpy failure is cladding hydride damage sustained prior to the test.

2. BWR design basis RIAs, such as the RDA, produce much different fuel responses than the limiting Anticipated Operational Occurrences (AOO) (usually the turbine trip without bypass). For this reason any new fuel failure mechanism(s) identified for the RDA, as may be associated with pellet rim effects, are not expected to apply to these AOOs.

2. CONTROL ROD DROP ACCIDENT

2.1 Scenario
The rod drop accident (RDA) in a BWR is a hypothetical design event. The assumed RDA scenario begins with the failure of the coupling between the control rod and its drive mechanism in such a way that the control rod becomes stuck in its fully inserted position. Subsequently the reactor operator selects the control rod and begins to withdraw the drive mechanism. Although the control rod mass is approximately 80 to 85 kg (176 to 187 lbm), it is assumed to remain stuck as the drive is withdrawn. The reactor operator fails to detect the lack of a response in the nuclear instrumentation and continues to withdraw the drive mechanism to some lower position in the core. At a later time, presumably at the worst possible time, the control rod becomes dislodged and falls out of the core.

2.2 Probability
A RDA probability study (Reference 2) concluded that a "reasonable (and quite possibly a conservative) estimate of the probability of having an RDA exceeding 280 cal/g is about $10^{-12}$ per reactor year". The NRC concurred with this study (Reference 3). It is important to note that this study assumed no credit for the rod worth minimizer which enforces the Banked Position Withdrawal Sequence (BPWS). The BPWS significantly reduces the worth of control rods during low power operations such as those that occur during cold and hot startups. The BPWS limits incremental control rod worths to an average value of 0.5% delta-k (Reference 4). Analyses have shown that an RDA with a rod worth less than 1% delta-k results in a total energy deposition less than 170 cal/g. Analyses have also shown that for power levels above 1% of rated thermal power, the consequences of an RDA are substantially reduced due to void generation.

2.3 Acceptance Criteria
The NRC acceptance criteria for RIAs from Section 15.4.9 of the NRC Standard Review Plan states: "The assumed failure threshold is a radially averaged fuel rod enthalpy greater than 170 cal/g at any axial location for zero or low power initial conditions."

For prompt power bursts it is appropriate that the 170 cal/g failure threshold be construed as a total gross energy deposition in the fuel. This is appropriate because the SPERT III tests (Reference 5) on which this number is based are prompt burst events without a substantial delayed power tail due to delayed neutrons. The total time for the prompt power burst is so short that very little energy is removed from the fuel. Thus analyses performed with and without heat transfer produce essentially the same results relative to the net energy deposition during the prompt power burst.
BWR RIA events such as the RDA include both the prompt power burst and a substantial power tail. Less than half of the total energy deposited in the fuel occurs during the prompt power burst. The remaining portion is deposited after the prompt power burst over a period on the order of the three to five seconds (until the power tail is truncated by the scram). This delayed energy was not included in the SPERT III tests so it is not inherently part of the basis for the 170 cal/g failure threshold. Therefore, in longer duration events that include the power tail and involve pellet expansion and cladding heatup, it is more realistic that the effects of heat transfer be considered. For such events a net maximum pellet average enthalpy serves as a realistic figure of merit.

The best-estimate maximum pellet average enthalpy values reported here are about 33% lower than those obtained using current GE licensing. The best-estimate results are net values since they reflect heat transfer from the pellet. The licensing results reported previously conservatively neglect the heat transfer from the pellet thus they are gross total energy deposition values.

3. METHODOLOGY

3.1 TRACG Computer Code

To quantify the energy deposition in the fuel due to an RDA, GE has performed detailed best-estimate evaluations using the TRACG computer code. The code is a best-estimate code that includes two-fluid thermal hydraulic models and a three-dimensional neutron kinetics model.

![Comparisons of TRACG Predictions to SPERT III Test Data](image)

TRACG has been benchmarked against the experimental control rod drop test performed at the SPERT III facility in 1965 (Reference 5). The code predictions for the peak power are higher than the data as shown in Figure A. Nevertheless, the total energy deposition in the fuel is predicted quite well by TRACG as shown in Figure B.
3.2 Core Modeling
Each bundle in the core is modeled individually for purposes of calculating the neutron flux and power distributions. Each bundle includes on the order of 25 axial cells, each with a height of approximately 0.15 m (6 in), with slight variations in the number and size depending on the specific bundle geometry.

The three-dimensional neutronics model is in essence a transient form of the quasi-static, three-dimensional neutronics model used by the GE 3-D BWR Simulator design and licensing code. The model is initialized from the 3-D BWR Simulator wrapup file created for the scenario of interest. For example, if initial cold critical conditions are desired, then the 3-D BWR Simulator is first run for these conditions to establish the initial power shape, control rod configuration, initial temperatures, etc.

3.3 Hydraulic Channels
The core is divided into hydraulic “channels” that represent a group of fuel bundles with similar characteristics. Thermal hydraulic information is calculated for each channel using a best estimate two-fluid model. The axial nodalization of a hydraulic channel is generally chosen to match the axial neutronics nodalization. The amount of thermal hydraulic detail and the spatial resolution of the hydraulic response in the horizontal plane is controlled by the number and distribution of the hydraulic channels in the model. For core wide events such as pressurization transients only a small number of channels are required. For highly localized events such as the RDA a much larger number of channels is used. For the case of the RDA it is important when setting up the channel assignments to consider the fuel lattice, bundle type, geometry of the bundle, exposure and the position of the bundle in the core.

3.4 Fuel Rod Groups
Within each channel any number of rod groups can be used. Each rod group simulates one or more fuel rods in that channel. Full-length and part-length fuel rods are grouped separately. Typically fuel rods containing burnable gadolinium are also grouped separately. A separate rod group is also set up for the water rods. When appropriate a separate rod group can be used to model the hottest rod.

3.5 Fuel Pellet Model
The transient radial heat conduction problem is solved for each axial cell in each rod group. Typically from 5 to 10 radial nodes are used to model the fuel pellet and additional radial nodes are used for the gap and the cladding. Fluid conditions are supplied from the channel model and the power distribution and amplitude are provided by the neutronics model. Direct moderator and structural heating are modeled to determine the amount of energy deposited in the fuel. The radial deposition of the energy in the pellet reflects the exposure-dependent fuel pellet radial power distribution caused by plutonium buildup on the pellet periphery. Heat transfer coefficients at the fuel pin surface are calculated to reflect conditions ranging from natural convection, forced convection, nucleate boiling, through boiling transition. The heat transfer in the pellet-clad gap is calculated from a best-estimate dynamic gap conductance model that includes the effects of fission gas, pellet and cladding thermal expansion and pellet-clad contact.

3.6 Realistic Model
The methodology described in the previous paragraphs is necessary to perform a best-estimate evaluation of the RDA. Whether this evaluation is realistic or conservative, plausible or implausible will depend not only on the models used but also on how the models are applied and the assumptions used to generate the inputs to these models.
4. ASSUMPTIONS

4.1 Key Assumptions

The approach used in making assumptions for this analysis was to choose limiting conditions that are slightly conservative yet realistic. The key assumptions are listed in the following paragraphs.

A large, high energy, control cell core (CCC) was selected since this is the type of core expected to have the most limiting grouping of highly-reactive fuel bundles adjacent to highly exposed bundles. The selected core is a core design (now operating) that meets all licensing and operational constraints.

Cold startup conditions were assumed. Minimum initial fuel temperatures ensure that the power increase is greater before the negative reactivity due to the Doppler feedback truncates the power increase. Minimum initial fluid temperatures ensure that the strong negative reactivity due to void formation is delayed (or avoided) thus resulting in a larger power increase.

End-of-cycle (EOC) conditions were assumed. The selected core was burned to EOC using the expected control rod movements (as designed) during the cycle. This scenario leads to a bundle-averaged exposure in the once burned fuel that corresponds roughly to the exposure at which all the gadolinium has been consumed and the bundle is near its point of maximum reactivity. The net effect is that the rod worths in the control cells that contain three once-burned fuel bundles in the core design analyzed are near or at their maximum values.

The reactor is assumed to be critical. These conditions are achieved by pulling rods at cold conditions until a critical configuration is reached. The importance of this initial condition is that it maximizes the net worth of any rods that subsequently will be dropped, while at the same time leading to a realistic control rod pattern.

4.2 Other Assumptions and Simplifications

All reactor conditions consistent with cold criticality corresponding to a startup at EOC conditions are assumed. For example: the recirculation pumps are operating at minimum speed with the flow control valve wide open; the steam dome pressure is at atmospheric conditions; air exists in the dome and the steam lines; there are no steam voids in the liquid; the feedwater flows and steam flows are zero; and the control rod pattern is the calculated pattern for these conditions.

The affected control rod is conservatively assumed to drop completely out of the core at a slightly conservative rate of 1.22 m/s (4.0 ft/s) compared to a value of 0.95 m/s (3.11 ft/s) assumed in licensing basis analyses.

The signal to initiate the scram occurs at the time of the power peak. This is a good approximation because of the rapid power rise for any event leading to a prompt criticality. Scram rod movement is conservatively delayed 0.5 seconds after the scram signal. The dropped rod is assumed to be broken in some way that prevents its reinsertion.

4.3 Banked Position Withdrawal Sequence (BPWS)

All GE-designed BWRs presently in service adhere to the Banked Position Withdrawal Sequence or its equivalent when operating at powers below the low power set point (LPSP, typically 10% to 20% of rated thermal power). BPWS is a method of moving control rods that limits the worth of control rods;
thereby, reducing the consequences of the RDA. BPWS is enforced for all power levels below the low power set point (LPSP) when starting up and when shutting down.

Adherence to BPWS is implicitly assumed in the RDA evaluations because failure of this system, together with the previously assumed equipment failures, would constitute more than a single equipment failure that is further compounded by multiple operator errors.

5. ANALYSIS and RESULTS

5.1 Initial Relative Power Distribution for Cold Critical Conditions

The control rods were withdrawn consistent with BPWS constraints during a cold startup. The initial power distribution at cold critical conditions is presented in Figure 1. Criticality is predicted for this core when control rods in the third rod group to be withdrawn are 0.61 m (2 ft) withdrawn (notch 8 of 48).

![Graph showing initial relative bundle power distribution](image)

The so-called "ring of fire" is clearly shown in Figure 1. For this case the relative bundle powers in the "ring of fire" are as much as a factor of four to five times the average bundle power for all bundles in the core. Lines of constant power are projected onto the x-y plane in Figure 1 so the areas of the greatest power gradients can be seen.

The core is a large core of 764 bundles that are predominantly of the GE11 fuel type. This core is considered to be more limiting because the "ring of fire" is more dramatic than is typical. This helps to maximize the control rod worths. The highest worth rods are those in the valleys or on the saddle between nearby power peaks.
Realistic Evaluation of Reactivity Insertion Accidents in BWRs

The highest in-sequence rod worths were identified and analyzed. The locations of these rods in the first quadrant are shown by the dots and the square in this figure. In addition the worst case out-of-sequence rod was also identified and analyzed (triangle in Figure 1). Initially each of these rods has corresponding symmetric rods in the other quadrants. Of course, this symmetry is not maintained once the rod drop occurs, thus a full core model was set up to model both the steady state and the transient conditions.

The results reported in the sections that follow are those obtained by dropping the highest worth in-sequence control rod located at the rod position marked by the square in Figure 1. The calculated total worth of this control rod is 0.79% delta-K ($1.40).

5.2 Fuel Channel Zone Assignments

Figure 2 shows the details of the fuel channel assignments for modeling the highest worth, in-sequence, rod drop (rod position (7,5)).

Considerable local detail is needed because the RDA power response is highly localized in the vicinity of the dropped rod (for all RDAs that produce a prompt criticality). In creating the model for this event it is very important to consider the bundle type, the bundle exposure, and the bundle position relative to the rod to be dropped. To accommodate these important elements, “zones” were set up around the rod to be dropped. The zones have been defined so that zone zero (0) is the zone immediately around the rod to be dropped, zone 1 corresponds to the bundles that are one bundle away from the dropped rod and zone 2 corresponds to bundles that are two bundles away from the dropped rod. In the channel assignments, the first digit of the channel number indicates the zone to which the channel is assigned.

![Figure 2: Fuel Channel Zone Assignments](image-url)
In this model the GE11 bundles are represented by the solid or striped patterns and the GE9 bundles are represented by the cross-hatched, brick-like patterns in Figure 2. Channels with numbers ending in 1 or 2 are GE9 bundles and all the other channels with numbers ending in 3, 6, 7 and 8 are GE11 bundles.

For both cases the darker shades correspond to the higher bundle-averaged exposures. The second digit in the channel numbers also indicates the approximate exposure of the bundle. The lower numbers indicate the bundles that were loaded earlier in the core life; thus, they are generally the ones with the higher exposures. For example, the 01, 11, 21, 31 bundles, etc., are all GE9 bundles with exposures in the range from 38 to 41 GWd/t. The 22 bundles are GE9 bundles at an exposure of 40.2 GWd/t while the 23 bundles are GE11 bundles also at the same exposure. The 36 bundles are GE11 bundles at various exposures as indicated by the different shades in Figure 2.

The following discussion will focus on the results for channels 01 and 07 in zone 0 immediately around the dropped rod. Channel 01 represents a GE9 bundle at 40.9 GWd/t. This is a highly exposed bundle that is being driven by the three highly reactive GE11 bundles next to it. The GE11 bundles represented by channel 07 have a bundle-averaged exposure of 16.7 GWd/t. These bundles are near their most reactive time in life since they are at roughly the exposure at which the last of the gadolinium has been consumed.

5.3 Total Power and Channel Power Responses
The total reactor power response normalized to rated power is shown in the left half of Figure 3 for the RDA involving the highest worth in-sequence rod. For the RDA the initial total reactor power is at 5 MWth corresponding to cold startup conditions. At one second the stuck rod begins to drop out of the core at the slightly conservative rate of 1.22 m/s (4.0 ft/s). At two seconds the rod has dropped 1.22 m (4 ft) out of the core which is far enough to yield enough positive reactivity for prompt criticality. The total power pulses to a peak value which corresponds to about 64% of rated reactor thermal power. This power pulse peaks at about 2.1 seconds. The scram signal is conservatively initiated at the time that the peak power occurs. Actual rod movement does not begin for another half second to account conservatively for the processing and mechanical delays inherent to the reactor protection system.

The power pulse is terminated by Doppler feedback as fuel temperature rises. This termination occurs before control rod movement due to the scram. At approximately 2.6 seconds an inflection in the total power is seen as the rods that were already deeply inserted begin to have an effect. Insertion of the fully withdrawn control rods is not completed until roughly 5.5 seconds.

The peak total reactor power value of 64% of rated power is concentrated in roughly a four-by-four array of bundles around the dropped rod. This highly localized power response is much different than the core-wide power response associated with a pressurization event in both its magnitude and its pulse width. Although pressurization events can produce peak total powers between 300% to 500% of rated power, this amount of power is distributed over all bundles in the core. Consequently, the peak channel power for an RDA is much higher than the peak channel power that is seen in a pressurization event such as a turbine trip with no bypass (TTNB). These facts are evident when the peak channel power responses are compared for the two events as in the right half of Figure 3. (The TTNB response was obtained from a previous analysis and was shifted in time to line up the power peak with the current results for the RDA.)
Realistic Evaluation of Reactivity Insertion Accidents in BWRs

For the presented RDA analysis the most reactive bundles around the dropped rod reach a peak power of 53 MWth each. This is about an order of magnitude above the full power operating value for these bundles. Keep in mind that this is occurring at a core flow that is only about 30% of rated flow. By contrast, the peak channel power for the previous TTNB analysis was 21 MWth or less than half the RDA value and occurred at 100% of rated core flow. Another important difference between the RDA and TTNB peak channel powers is the width of the power pulses. The RDA power pulse (75 msec) is more than four times shorter duration than the power pulse for the TTNB event.

5.4 Sensitivity to Rim Power Peaking

The four fuel bundles adjacent to the dropped rod are modeled by two channels as described previously in Section 5.2 and indicated pictorially in Figure 2. Radially averaged fuel pellet enthalpies were calculated for each axial cell and each rod group in each of these channels. The maximum of these values for the two channels adjacent to the dropped rod are plotted in Figure 4. The solid lines (curves 1 and 2) and the dashed lines (curves 3 and 4) correspond to two different analyses that were performed using different rim power peaking factors. Curves 1 and 3 in Figure 4 are for channel 01 which corresponds to the single GE9 bundle at 40.9 GWD/t. Curves 2 and 4 in Figure 4 are for channel 07 which represents the three GE11 bundles at 16.7 GWD/t.

The results presented in Figure 4 were obtained using a model for the fuel pellet that contained ten radial nodes. The outermost radial node was constructed to have a thickness of 100 microns. This yields a rim volume that is a little less than 4% of the total pellet volume for GE9 and a little more than 4% of the total pellet volume for GE11 due to the smaller pellet diameters for GE11 fuel. The remaining nine interior nodes in the pellet were set up as rings of equal volume.
Due to nuclear self-shielding the power density in the outer rim is higher than the pellet average power density. Although the rim may account for only about 4% of the volume of the pellet, at higher pellet exposures the rim can account for up to about 7% of the power of the pellet. The rim power density increases as the exposure increases. In this particular example the high exposure GB9 fuel at 40.9 GWD/t exposure had a rim power density that was about 22 percent higher than the rim power density in the low exposure GE11 fuel at 16.7 GWD/t.

The sensitivity of the maximum radially averaged pellet enthalpy to the pellet radial power distribution was evaluated as described below. The calculations were first performed using a best-estimate calculated radial power density in the pellet. These results are labeled “nominal” in Figure 4 and plotted as the solid curves 1 and 2 for the GB9 and GE11 channels, respectively. In the second set of calculations, the rim power effect was accentuated by doubling the calculated nominal ratio of rim power density to centerline power density. For example, the nominal rim-to-centerline power density ratio of 1.9 for the GB9 bundle becomes 3.8 in the rim-accentuated calculation. The results for the rim-accentuated case are plotted in Figure 4 as the dashed curves 3 and 4 for the GB9 and GE11 channels, respectively.

Comparison of the dashed curves in Figure 4 for the accentuated rim case to the solid curves for the nominal rim case reveal an interesting result. When more of the power is generated in the rim the maximum pellet averaged enthalpy decreases slightly. Obviously, if a larger fraction of the power is produced near the surface of the pellet then less power must be produced in the interior part of the pellet. Because power produced at the pellet’s edge yields a greater temperature gradient near the edge of the pellet, energy produced in the pellet is more quickly conducted through the gap and cladding to the fluid. The net effect is that the retained energy per unit mass in the pellet is lower. One can see from the results in Fig-
Figure 4 that increasing the rim power peaking actually results in a slight decrease (and thus benefit) in the maximum pellet averaged enthalpy.

Another key point to be made from Figure 4 is that the maximum enthalpy of 16 cal/g in the high exposure bundle is substantially lower than the 35 cal/g seen in the low exposure bundle even though the two bundles are both in the same control cell as the dropped rod. Although the bundles are of a different type, it has been verified that the maximum enthalpy values are only weakly sensitive to the bundle type. The bulk of the difference between the two maximum pellet averaged enthalpy values is due to the differences in depleted enrichment at the different exposures. Regardless of the bundle exposures, the values are very low compared to the 170 cal/g RDA licensing criteria specified for perforation of the cladding.

It should be pointed out that these results were obtained using a flat pin-to-pin power profile in the horizontal plane within the bundles. To account for the local pin power peaking factor it is conservative to multiply these results by the worst case local pin peaking factor. Application of the local pin peaking to these results is conservative because at least some fraction of the additional power will be removed by the fluid due to heat transfer. Even when the peak values in Figure 4 are increased to account for an assumed local pin power peaking factor, the resulting values are still a factor of approximately 4 to 8 below the 170 cal/g RDA licensing criteria. For the best-estimate calculation assuming a full drop of a high-worth, in-sequence control rod the peak enthalpy value for the most highly-reactive fuel in the core is only about 45 cal/g. The peak pellet average enthalpy is less than 20 cal/g at a bundle average exposure of 40 GWd/t.

5.5 Zone of Influence

The maximum radially averaged pellet enthalpies for a given bundle exposure occur in the control cell around the dropped rod. This is due to the fact that the power response is highly localized around the dropped rod for any rod worth that results in a prompt criticality. The degree to which the power response is localized to the region around the dropped rod can be seen in Figure 5. The individual channel power responses shown in Figure 5 were scaled to the core average channel power at 100% power. Power responses for selected channels are plotted by zones as one moves away from the dropped rod. The channels to be plotted were selected so that bundles with similar bundle-averaged exposure could be compared on a zone-by-zone basis.

Recall that zone 0 corresponds to the four bundles in the control cell around the dropped rod. Zone 1 includes the twelve bundles that are one bundle away from the dropped rod and zone 2 includes the 20 bundles that are nominally 2 bundles away from the dropped rod. The details of the zone assignments for the presented case are shown pictorially in Figure 2. In zones 3 and beyond it is not possible to compare GE11 bundles that have exactly the same exposure because a coarser grouping of bundles into channels was used beyond zone 2. In zones 3, 4 and 5+ the GE11 bundles have a bundle-averaged exposure of 25 GWd/t corresponding to roughly the average exposure of all GE11 bundles in this core instead of the 17 GWd/t exposure for the GE11 bundles selected in zones 0, 1 and 2. For the GE9 bundles this problem did not exist because the average exposure for all GE9 bundles in the core was roughly 40.3 GWd/t.

The plots in Figure 5 show that channel power responses outside the four-by-four array of bundles around the dropped rod decrease rapidly as the distance from the dropped rod increases. Three bundles away from the dropped rod the power pulses are less than 20% of what they are in the bundles adjacent to the dropped rod. The plots also confirm that the power responses for the higher-exposure bundles are
about half the magnitude of those for the lower-exposure bundles. This relationship is maintained on a zone-by-zone basis as the distance away from the dropped rod increases.

### 5.6 Fuel Enthalpy Dependence on Bundle Average Exposure

As discussed in the previous section, bundle powers decrease on a zone-by-zone basis as the distance away from the dropped rod increases. The same trend is expected for the maximum pellet averaged enthalpies in these bundles because the value of the maximum pellet averaged enthalpy in a bundle is dominated by the overall power response of the bundle. The trend in the calculated results is shown in Figure 6. It can be seen that the calculated maximum pellet averaged enthalpies for a given exposure range decrease on a zone-by-zone basis as the distance away from the dropped rod increases.

The overall power response of a bundle consists primarily of a “driven” component and a “reactive” component. The “driven” component is that due to the external neutron flux imposed on a bundle by the other bundles in the region. This “driven” component is essentially the same for all bundles in the same zone. Thus by comparing the bundle responses within a particular zone the “reactive” power response of a particular bundle can be inferred from its total response. Figure 6 has been designed to facilitate such a comparison.

The results in Figure 6 show that within a particular zone the maximum pellet averaged enthalpies are correlated to the bundle average exposures. This result is due to the fact that the “reactive” power (hence enthalpy) response of a bundle depends primarily on the reactivity of the bundle which depends mainly on the average exposure of the bundle. Other effects such as the local axial power shape and the axial exposure history of the bundle have some impact but they are of second order importance for the RDA.
Thus for the RDA it is possible to make some observations about how the maximum pellet averaged enthalpy in a bundle will change for different bundle average exposures.

Bundles with average exposures less than 10 GWd/t have lower maximum pellet averaged enthalpies because not all their gadolinium has been consumed. Bundles in the 10 to 20 GWd/t exposure range yield the highest maximum pellet averaged enthalpies because most or all of their gadolinium has been consumed; yet substantial amounts of U-235 remain. As bundle average exposures increase beyond 20 GWd/t the maximum pellet averaged enthalpies in a bundle progressively decreases as its U-235 content is consumed. As the higher exposures are reached the decline in the fuel enthalpies with exposure becomes less steep as the fraction of power produced by plutonium fissions increases. The trend for maximum pellet average enthalpies versus exposure is roughly parallel to the trend for bundle infinite lattice multiplication factor (k–infinity) versus exposure.

5.7 Effect of Heat Transfer

The sensitivity of the maximum radially averaged pellet enthalpy to the pellet radial power distribution has already been discussed in Section 5.4. It was seen that the maximum pellet averaged enthalpies are not strongly influenced by even large changes in the rim peaking. In fact a slight decrease in the maximum pellet enthalpies was seen as the rim peaking was increased. This was attributed to the fact that heat transfer plays an important role in redistributing the energy in and from the pellet after the prompt power burst. As shown in Figure 4, the prompt power burst accounts for less than half of the overall enthalpy increase in the pellet. This can be understood physically by considering the amount of energy in the prompt power pulse (see Figure 3) and comparing this to the energy deposited in the pellet in the one
second time period after the prompt power pulse. Although the power pulse is large, it is also quite narrow as shown in Figure 5.

The effect of heat transfer in determining the maximum pellet average enthalpies was assessed using the approach now described. The dropped rod transient calculations were performed both with and without fuel pin heat transfer. The results presented thus far have been from calculations using best estimate pellet-to-cladding and cladding-to-fluid heat transfer models. For the calculation now being introduced, heat transfer from the fuel pellet was shut off for all the fuel pins in the core by applying a zero pellet-to-cladding gap conductance. The calculation with the accentuated rim peaking and with heat transfer (HT) is the base case to which the new results without heat transfer (No HT) are compared.

![Graph](image)

**Figure 7: Comparisons With and Without Heat Transfer Through the Gap**

Radially-averaged fuel pellet enthalpies were calculated for each axial cell and each rod group in each channel. The maximum of these values for the two channels adjacent to the dropped rod were plotted as a function of time. The maximum pellet average enthalpies were determined in the same way as described for Figure 4 in Section 5.4. In fact, curves 3 and 4 from Figure 4 are replotted in Figure 7 (also as curves 3 and 4) to facilitate the comparison. The solid lines (curves 3 and 4) and the dashed lines (curves 1 and 2) correspond respectively to the two different analyses that were performed with and without heat transfer. Curves 1 and 3 in Figure 7 are for channel 01 which corresponds to the single GE9 bundle at 40.9 GWD/t. Curves 2 and 4 in Figure 7 are for channel 07 which represents the three GE11 bundles at 16.7 GWD/t.

The effect of heat transfer on the calculated maximum pellet average enthalpies is shown in Figure 7. The calculated peak pellet average enthalpies using best-estimate methodology with heat transfer (solid
curves 3 and 4) are about 33% lower than the peak pellet average enthalpies calculated without heat transfer (dashed curves 1 and 2).

5.8 Energy Redistribution Within the Pellet

The importance of energy removal from the pellet and the redistribution of energy within the pellet can be seen by plotting the radial temperature profile at various times following the power pulse. For the purposes of brevity, only the results at the location of the maximum pellet average enthalpy in the entire core are presented. The maximum value occurs axially in cell 20 out of 25 in channel 07 corresponding to the GE11 bundles at 16.7 GWD/t adjacent to the dropped rod. The temperatures in the fuel pin at this location are plotted in Figure 8 at various points in time. The case with accentuated rim peaking was selected for presentation because the segregation between curves is greater. However, even with the accentuated rim power peaking the temperatures near the pellet periphery are not significantly higher than the pellet average temperature.

The curves in Figure 8 occur in pairs. There is one pair of curves for each point in time. One curve in each set is for the calculated results obtained with the heat transfer model activated (HT). The second curve in each pair is for the calculation where the gap conductance was zeroed (No HT). These latter curves all terminate outside the pellet very near the initial temperature of about 293 K since only that small fraction of the total energy that is directly deposited in the fluid moderator leaves the pellet.

In Figure 8 the first pair of curves at 2.1 seconds coincides with the time of the power peak. Even at this early point in time the differences between the HT and No HT curves reveal that heat transfer to the fluid

![Figure 8: Radial Temperature Distributions in the Fuel Pellet at Location of Maximum Pellet Average Enthalpy](image)
has already begun to reduce the temperature in the rim of the pellet. For both the HT and No HT cases
the interior part of the fuel pellet is at this point serving as a heat sink as energy from the hotter exterior
part of the pellet is conducted back into the cooler interior part of the pellet.

By 2.6 seconds, roughly 0.5 seconds after the power peak, the temperature gradient in the pellet rim has
changed substantially. At this time the local enthalpy and temperature in the rim are near their peak val-
es for the case with realistic heat transfer. Note that these local maximum values for enthalpy and tem-
perature in the rim are lower than the peak pellet average enthalpy and temperatures that will not occur
until approximately two seconds later.

By 2.6 seconds it is apparent that the temperatures in the pellet interior are about 50 K higher for the case
with heat transfer than they are for the case without. This is the result of a higher calculated power pulse
for the case with heat transfer as a result of an overall reduction and delay in the Doppler feedback. The
Doppler feedback is more strongly affected by temperatures that are closer to the pellet surface than it is
by temperatures closer to the center. Firstly, there is a greater volume associated with the temperatures
closer to the pellet surface. Secondly, in terms of neutron flux within the pellet, the temperatures near the
surface of the pellet are weighted more heavily in determining the neutron effective temperature that goes
into calculation of the Doppler feedback. Together these factors combine to increase the weight of the
temperature near the surface of the pellet relative to the temperatures in the interior part of the pellet.
Consequently, the lower temperatures near the surface of the pellet for the case where heat transfer is con-
sidered produce a lower pellet-averaged, neutron effective temperature which decreases and delays Dop-
pler feedback and results in a higher power pulse. This higher power pulse results in higher temperatures
in the interior part of the pellet where removal of energy is delayed by thermal diffusion until the inverted
radial temperature gradient has evolved into a negative radial gradient conducive to heat removal.

By 4.7 seconds, 2.6 seconds after the power peak, a negative thermal gradient has been well established
in the pellet for the case with HT. Enough energy has been removed from the interior part of the pellet to
more than compensate for the higher power pulse for the case with HT. The pellet averaged enthalpy is
at or very near its peak value for either the HT or No HT cases. The average enthalpy for the case with
heat transfer will begin to slowly decrease and the negative thermal gradient will begin to flatten as energy
continues to be removed from the pellet. For the case without heat transfer, the positive thermal gra-
dient will also continue to flatten as heat is conducted from the hotter exterior part of the pellet back
toward the center of the pellet. This will not yield any appreciable change in the pellet average enthalpy
because the value has already approached its maximum asymptotic value.

5.9 High Exposure Fuel Responses during an RDA
At higher exposures the pellet-clad gap size is reduced as a result of normal pellet and cladding expan-
sion and deformation mechanisms. For the case of pellet-clad contact during normal steady state opera-
tion at high exposure, it has been postulated that an RDA may produce excessive cladding stresses and
strains as the pellet expands in response to the rapid energy deposition. This hypothesis was evaluated
for the GE9 fuel at 40.9 GWd/t. The temporal behavior of the maximum pellet power and corresponding
local heat transfer coefficient at the clad surface from the TRACG analysis were used as input to a more
detailed best-estimate fuel rod mechanics model. For this calculation, an assumed worst-case local pin
peaking factor was applied to the average pin power values generated by TRACG. The resulting temper-
ature responses of the pellet and cladding from this more detailed fuel rod model were found to compare
favorably with the responses obtained from TRACG (taking into account the local pin peaking). The
The results presented thus far have all been for a control rod worth of $1.40 (0.79%). This is the highest rod worth that could be identified for in-sequence control rods in the selected core. It is apparent from the results that have been presented that for an RDA involving a rod of this worth that the 170 cal/g limit for clad perforation is not approached.

In an earlier RDA analysis performed for a small BWR core using GE licensing methods, an out-of-sequence rod with a worth of 1.07% was dropped. For this earlier analysis the peak pellet average enthalpy in the most reactive fuel jumped initially by 40 cal/g and eventually, in the assumed absence of heat transfer, reached 117 cal/g about 2.4 seconds after the power pulse (Reference 6). These peak enthalpy
depositions occurred in a fuel bundle with an average exposure of 11.4 GWd/t. In order to compare these earlier results to the current results, a multiplier was applied to the calculated power in the most reactive bundle in the current analysis in order to achieve a prompt jump of 40 cal/g. The calculations were repeated using this multiplier and the current methods for the cases with and without heat transfer through the gap.

The new results with and without heat transfer through the gap are presented in Figure 10. As expected a prompt jump in pellet average enthalpy near 40 cal/g is achieved. The results for the case without heat transfer through the gap yield a peak enthalpy around 117 cal/g, thereby verifying the results that were obtained previously when the current GE licensing methods were used. As before, the results with pellet-to-clad heat transfer are about 33% lower than the results obtained when energy removal from the pellet is neglected. Note that this out-of-sequence RDA yields a prompt enthalpy increase that is approximately twice the value presented earlier for a drop of the highest worth in-sequence rod. This result further illustrates the conservatism in the current GE RDA licensing methods.

6. DISCUSSION of FUEL ROD THERMAL–MECHANICAL BEHAVIOR

6.1 Implications of the CABRI Test Results

The results from a recent experiment conducted by Electricité de France (EdF) at the sodium cooled CABRI facility has suggested that the failure threshold during an RIA may be significantly reduced at high exposure (Reference 1). The CABRI experiment (test rod Na1) was performed on a refabricated section of a commercial PWR fuel rod irradiated to approximately 63 GWd/t segment averaged exposure. The test power pulse was initiated at a system temperature of approximately 280 C (approximately 15 cal/g

![Figure 10: Conservative RDA Results Using Licensing Assumptions With and Without Heat Transfer Through the Gap](image-url)
initial fuel enthalpy). In a period of less than 100 msec following the power pulse a peak radially averaged fuel enthalpy of approximately 116 cal/g was reached. (Contrast this to the design basis RDA discussed in Section 5.10 where greater than three seconds are needed to reach approximately the same total energy deposition.) The first indication of failure was reported to be early in the power burst at a radially averaged fuel enthalpy of approximately 30 cal/g. On this basis some investigators speculate that the cladding failure threshold at high exposures may be as low as approximately 15 cal/g energy deposition.

Some investigators have postulated that the known formation of a highly porous region in the rim of the fuel pellet at elevated exposures may have somehow resulted in the low energy failure of test rod Na1. It has been further postulated that such "rim effects", if active during an RIA, could also significantly affect fuel performance during rapid, pressurization-type Anticipated Operational Occurrences (AOO).

6.2 Anticipated Operational Occurrences (AOO)

An AOO is typically an event which results in a subprompt critical transient; whereas, the limiting RIA results in a super–prompt critical transient. Consequently, the RIA and AOOs are markedly different events from a temporal energy deposition perspective (see Figure 3). Worst case AOOs typically are core wide transients characterized by a single power pulse with an amplitude of approximately four to five times rated total power and a pulse width on the order of 300 to 400 msec halfway up the pulse. By comparison, the calculated localized power pulse for a BWR RDA can have a much higher amplitude with a half–height pulse width of only 50 to 100 msec. Therefore, the conditions affecting fuel performance during an RIA are markedly different than those affecting fuel performance during an AOO.

6.3 Differences in RDAs and AOOs

A useful perspective for the rapid heatup of the pellet rim for an RIA event compared to an AOO pressurization event can be gained from the peak fuel pellet surface temperatures plotted in Figure 11. Curve 1 in Figure 11 shows the calculated fuel pellet surface temperature for a BWR rod drop accident resulting in a peak total fuel enthalpy increase of 117 cal/g of which 40 cal/g is due to the prompt power burst. (Note that this out-of-sequence RDA yields a prompt enthalpy increase that is about double the value presented earlier for a drop of the highest worth in–sequence rod.) Curve 1 in Figure 11 shows a 1575 K temperature increase that reflects full temperature redistribution effects within the fuel rod but no fuel rod–to–coolant heat transfer. Curve 2 in Figure 11 shows a 921 K increase for the same RDA event, but with calculated realistic fuel rod–to–coolant heat transfer. For comparison, curve 3 in Figure 11 shows the fuel surface temperature response for a bounding rapid pressurization event AOO (turbine trip without bypass) that was performed using standard analysis assumptions. It is clear from Figure 11 that the pellet surface temperature increase of 53 K for the bounding AOO turbine trip is not comparable to the large temperature increases seen for the RDAs. Therefore, it is concluded that any fundamental new failure mechanism that may be suggested by the CABRI experiment, such as pellet “rim effects”, does not translate directly to a corresponding fuel performance issue directly applicable to BWR normal operation and AOOs.

6.4 Assessment of Pellet Rim Effects

The role of the pellet rim during an RIA is postulated by some investigators to be a rapid swelling of the fission gas bubbles contained in the pellet rim due to the significant rim temperature increase. The radial power distribution across the high exposure fuel pellets is characterized by a high degree of peaking near the pellet surface due to plutonium buildup. During the rapid power increase that occurs during an RDA, the fuel temperatures near the pellet surface increase rapidly then gradually subside as a result of energy
Redistribution in the pellet and heat transfer from the pellet through the gap and cladding to the coolant. This increased pellet rim gas bubble expansion is postulated to result in pellet-cladding contact and excessive loading of the fuel rod cladding.

This postulated effect was investigated for the CABRI tests Na1 (116 cal/g total energy deposition, 63 GWd/t), Na2 (202 cal/g, 33 GWd/t) and Na3 (116 cal/g, 53 GWd/t). The conditions for these tests were modeled to enable a comparison of calculated and measured cladding deformation. The cladding strain calculations were performed by assuming that the fuel pellet and cladding were in contact at the end of the commercial PWR base irradiation while at operating temperature. Slight initial gaps due to thermal contraction to the cold conditions at the start of the CABRI tests were then calculated.

The cladding permanent deformations during the CABRI power pulses were then calculated. For this calculation, the only fuel pellet expansion mechanism considered was simple linear thermal expansion which was assumed to occur about the cracked pellet wedge apex. From these results the cladding hoop strains were predicted. The results of these predictions are compared to the measured hoop strains in Figure 12. The comparisons between measured and predicted cladding permanent strains are within normal prediction capability even without consideration of the postulated pellet rim gaseous bubble swelling. Furthermore, there is no significant difference in prediction capability between the lower exposure and higher exposure fuel rods. Therefore, it is concluded that the postulated mechanism of pellet rim gas bubble (if it exists) did not contribute significantly to the fuel performance during these tests.
6.5 Discussion of the CABRI Test Results

These results presented in Section 6.4 do not support the pellet rim gas bubble expansion mechanism as a viable explanation for the low enthalpy failure of the CABRI test rod Na1. In fact, the validity of the Na1 test data is further questioned by the subsequent successful testing of test rods Na3 and Na5 in the CABRI reactor. In these tests, refabricated commercial PWR fuel rod sections irradiated to approximately 53 GWD/t and 63 GWD/t reached respective peak fuel enthalpies of approximately 117 cal/g and 120 cal/g and did not fail. Both of these fuel rods are expected to have developed pellet rim conditions comparable to the Na1 rod which had been irradiated to approximately 63 GWD/t. Therefore, the failure of the Na1 rod cannot be attributed to the dynamics of the pellet rim.

The Japan Atomic Energy Research Institute (JAERI) has recently reported (Reference 7) results of their investigations of a relatively low energy deposition failure of a mid-exposure fuel rod (approximately 21 GWD/t) tested at the Nuclear Safety Research Reactor (NSRR) in Japan. The investigation revealed that the test rod was damaged prior to the test, as evidenced by pre-test eddy current examination, and failed during the RIA test at the locations of pre-test damage. Destructive examination of the failure sites revealed localized hydride damage that is believed to have occurred during the fuel rod base irradiation as a result of as-fabricated fuel rod fill gas impurities. Based on the limited metalographic examination results that have been made available for the Na1 fuel rod, pre-test hydride damage is also evident and may well have been the cause of the low energy deposition failure.
6.6 Summary of Fuel Thermal Mechanical Considerations

The available evidence does not support a new high exposure failure mechanism (such as pellet rim fission gas bubble swelling) as a root cause of the Na1 low energy deposition failure. Subsequent high exposure testing at CABRI (Na3, Na5) further reinforces this point. With the limited information made available on the Na1 test and destructive examination, it is considered more probable that the Na1 rod was damaged by hydriding prior to the RIA test and that this hydride damage resulted in the low enthalpy failure. Such hydride damage is not prototypical of GE BWR fuel; therefore, this test result is not applicable to GE BWR fuel. Even if a new high exposure failure mechanism is discovered for RIAs involving short duration power pulses, there is no reason to anticipate such a mechanism for normal AOOs due to the significant and fundamental differences in the fuel responses for AOOs compared to RIAs.

7. CONCLUSIONS

The design basis reactivity insertion accident for a BWR is the control rod drop accident. Realistic evaluation of the RDA reveals that current BWR fuel designs have substantial margins to the current 170 cal/g fuel average enthalpy limit for cladding perforation. The margins are primarily due to the inherent feedback mechanisms of the BWR and the use of BPWS to limit the control rod worth of rods that can be dropped.

Energy deposition in the fuel decreases with increasing exposure, for bundles with averaged exposures that are greater than the exposure at which the gadolinium in the fuel has been consumed. Large energy depositions in fuel that has been highly exposed are not possible. For the limiting case analyzed, best-estimate results assuming a full drop of the highest worth in-sequence control rod, the peak pellet average enthalpy is less than 20 cal/g at a bundle average exposure of 40 GWd/t.

The fuel pellet radial power distribution does not significantly impact the calculated maximum value for pellet averaged enthalpy during an RDA. Increasing the pellet rim power peaking has been found to actually produce a slight reduction in the maximum pellet averaged enthalpy.

The zone of influence in terms of the power responses of bundles around the dropped rod has been found to be small. The channel power responses outside the four-by-four array of bundles around the dropped rod decrease rapidly as the distance from the dropped rod increases. Three bundles away from the dropped rod the power pulses are less than 20% of what they are in the bundles adjacent to the dropped rod. The zone of influence becomes even smaller as exposure increases. Even if all fuel in the affected zone were postulated to fail, significant margin to the offsite radiological dose limits would still exist.

Heat transfer within and external to the fuel has a significant impact on the maximum pellet averaged enthalpy. For best-estimate analyses that include heat transfer it has been shown that the calculated maximum pellet average enthalpies are about 33% lower than the values obtained when energy removal from the pellet is neglected.

Present licensing procedures and methods used by GE are very conservative for two main reasons: (1) higher control rod worths are assumed to allow relaxation of the BPWS restrictions for a limited number of control rods, and (2) increased fuel rod-to-coolant heat transfer during an RDA event are neglected.

Design basis RIAs such as the RDA are fundamentally different from the limiting AOO RIA event (usually the turbine trip without bypass). The RDA leads to very rapid energy depositions that are highly localized to a few bundles adjacent to the dropped rod. AOO pressurization events on the other hand de-
posit significantly less energy to an individual fuel bundle and that energy is deposited at a significantly reduced rate. Therefore, reduced fuel performance during an RIA, if it exists, cannot be translated directly to a performance issue during normal steady-state operation of AOOs.

The measured cladding strains for the CABRI Na1, Na2 and Na3 tests can be predicted without postulating a new high exposure mechanism such as pellet rim fission gas bubble swelling. The available evidence does not support such a new high exposure failure mechanism. The more likely cause of the Na1 low enthalpy failure is cladding hydride damage sustained prior to the CABRI RIA test. Such hydride damage is not prototypical of GE BWR fuel; therefore, no general implications from this single RIA test result can be applied to GE BWR fuel.

8. REFERENCES


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**CSNI Specialist Meeting on Transient Behaviour of High Burnup Fuel**

Cadarache, France, 12th-14th September 1995

**TABLE OF CONTENTS**

Session IV : SEPARATE-EFFECT TEST AND ANALYSIS

Co-Chair: V. LANGMANN and A. DELBRASSINE

Summary of the Session ................................................................. 403

« High Burnup Phenomena - Results from Experiments in the Halden Reactor »
W. WIESENACK .................................................................................. 405

« Tensile Properties of Irradiated Zircaloy-4 Cladding Submitted to Fast
Transient Loading »
M. BALOURDET, C. BERNAUDAT ..................................................... 417

« Influence of Locally Concentrated Hybrides on Ductility of Zircaloy-4 »
F. NAGASE, K. ISHIJIMA, T. FURUTA ............................................ 433

« A 2D-3D Finite-Element Approach of Fuel Rod Thermomechanical
Behaviour During a RIA »
C. BERNAUDAT, J.-P. BERTON, P. PERMEZEL ................................. 445

« Evaluation of RIA Experiments and their Impact on High-
Burnup Fuel Performance »
Y.R. RASHID, R.O. MONTGOMERY, O. OZER, R. YANG,
S. YAGNIK, L. AGEE ......................................................................... 471

« Impact of Fission Gas on Irradiated PWR Fuel Behaviour at Extended
Burnup under RIA Conditions »
F. LEMOINE, F. SCHMITZ ............................................................... 493

« Modelling of Phenomena Associated With High Burnup Fuel
Behaviour During Overpower Transients »
H.E. SILLS, V.J. LANGMANN, F.C. IGLESIAS ................................. 515

« High-Burnup Modeling Changes to NRC Fuel Performance Codes
that Impact Reactivity Initiated Accidents »
C.E. BEYER, D.D. LANNING, L.J. SIEFKEN .................................. 533

Invited Paper from Host Organisation

« The Project to Implement into CABRI a Pressurized Water Loop.
Motivations and Objective of the Future Test Program »
J. FURLAN, M. HAESSLER .............................................................. 551

401
SUMMARY OF SESSION IV: SEPARATE-EFFECT TESTS AND ANALYSIS

A significant body of experimental work, both separate- and integral-effect tests, and analytic work, has been presented which relates to the behaviour of high burnup LWR fuel during rapid power pulses such as those in Reactivity Initiated Accidents (RIAs). It would appear that all of the important phenomena and factors, affecting this behaviour, have been identified. These important phenomena and factors are as follows:

1. The transient thermal/mechanical behaviour of highly irradiated clad. Factors of importance include the:
   i. material type and fabrication route;
   ii. degree and characteristics of oxidation, with emphasis on the extent of spallation;
   iii. degree and orientation of hydriding with a focus on hydride gradients; and
   iv. strain rate sensitivity of the clad.

2. The transient thermal/mechanical behaviour of the high burnup « rim » region of the fuel (i.e. enhanced rate of fuel heatup, grain-boundary gas bubble pressurisation, fuel microcracking, transient gas-release to the free volume and fuel swelling).

3. The characteristics of the RIA power pulse (i.e. pulse width, gross energy deposition into the fuel, transient heat removal to the coolant and consequent fuel enthalpy).

4. The fuel conditions immediately prior to the accident (i.e. the degree of fuel/clad gap closure, the fuel radial power profile, the radial variation in fuel thermal conductivity and porosity, and the resultant fuel radial temperature distribution).

5. Longer term (i.e. post-DNB) transient fission gas/volatiles release and consequent fuel swelling.

In order to quantify the fuel energy deposition required to result in high burnup fuel clad failure, the major considerations are the loss of clad ductility and the factors which determine the transient thermal behaviour and loading of the clad. This first point (listed previously) related to the loss of clad ductility. The latter four points relate to the transient thermal behaviour and loading of the clad.

It would appear that if extended burnup fuel has clad characterised by high hydrogen contents (i.e. localised hydride formations such as « sunbursts »and/or hydride distribution gradients), then clad failure will occur at reduced energy depositions. Thus, from the point-of-view of clad failures during RIAs, obtaining quantitative information on the range of conditions that lead to a significant loss of clad ductility is of primary importance. It is apparent, from the papers presented, that this information has been, is being, and should continue to be obtained through well-characterised separate-effects tests in various countries.
Existing programs that provide insight into the previously discussed factors include:

i. ex- and in-reactor investigations of the burnup dependence of fuel thermal conductivity, and the conductivity of "rim structure" fuel;
ii. ex-reactor tests on the mechanical properties of clad from high burnup rods as a function of corrosion, hydriding, fast neutron fluence and strain rate;
iii. neutronic and thermal-hydraulic code calculations to assess realistic energy depositions and pulse widths for RIAs; and
iv. detailed post-test examinations of failed and intact RIA-tested rods, including measurements of the radial distributions of fission gas release and fuel swelling and clad strains and cracking.

Additional insight into the behaviour of highly irradiated fuel rods in RIAs would be obtained via further work on:

i. the definition of radial power peaking in high burnup fuel;
ii. analysis of thermal diffusivity/thermal conductivity measurements;
iii. further correlation between oxide layer spallation, hydride concentration and distribution, and clad ductility loss under prototypical RIA conditions, including recovery of clad ductility as a function of time at temperature; and
iv. in-reactor RIA tests at prototypic neutronic, coolant and pre-transient conditions.

It is a more complex problem to quantitatively assess the potential for, and/or extent of, dispersion of highly irradiated fuel as a function of energy deposition in an RIA. In this case, it is important to understand the interplay between all the various factors affecting the transient, thermal/mechanical behaviour of the high burnup UO₂ fuel.

If a more representative, less restrictive understanding of the potential for high burnup fuel dispersion during RIAs is desired for LWR licensing analyses, then further, more prototypic in-reactor tests would contribute to this understanding and licensing code validation. If required, these more prototypic in-reactor tests should be performed under conditions more representative of those expected in LWR RIAs (i.e. wider power pulse widths, representative pre-transient fuel and transient coolant conditions).
HIGH BURNUP PHENOMENA - RESULTS FROM EXPERIMENTS IN THE HALDEN REACTOR

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ABSTRACT

The fuels and materials testing programmes carried out at the OECD Halden Reactor Project are aimed at providing in-pile data in support of a mechanistic understanding of phenomena, especially as related to high burnup fuel. The investigations are focused on identifying long-term property changes, thus the results can be used to establish the starting conditions of a transient. The paper presents data and findings on phenomena which may relate to the RIA behaviour of high burnup fuel.

The fuel-cladding gap has an influence on both thermal and mechanical behaviour. Improved gap conductance due to gap closure at high exposure is observed even in the case of a strong contamination with released fission gas. On the other hand, pellet-cladding mechanical interaction, which is measured with cladding elongation detectors and diameter gauges, is increasing after a phase with less interaction. These developments are exemplified with data showing changes of fuel temperature, hydraulic diameter and cladding elongation with burnup.

Fuel conductivity degradation is observed as a gradual temperature increase with burnup. This affects temperatures and heat flow especially after the initial phase of the transient.

The Halden Project's data base on fission gas release shows that the phenomenon is associated with an accumulation of gas atoms at the grain boundaries to a critical concentration before appreciable release occurs. This is accompanied by an increase of the surface-to-volume ratio measured in-pile in gas flow experiments. The impact on RIA behaviour may stem from a progressive weakening of the fuel, making it more susceptible to intergranular fracture when thermal stresses are induced. A typical observation at high burnup is also that a burst release of fission gas may occur during a power decrease. This implies that released gas is trapped in fuel cracks, exerting pressure on the fuel fragments and the cladding.

Gas flow and pressure equilibration experiments have shown that axial communication is severely restricted at high burnup. This means that gas in fuel cracks and the gap cannot escape to the plena when heated to high pressure during a transient, but will rather contribute to increase the local load on the cladding.
1. INTRODUCTION

Safe and economic nuclear power generation requires a fundamental knowledge of fuel behaviour in different situations. The research programmes carried out at the OECD Halden Reactor Project have for more than thirty years addressed areas of particular interest to the nuclear community and provided significant contributions to the understanding of LWR fuel behaviour. From the beginning, fuel performance and reliability investigations were supported by the development and perfection of in-core rod instruments. The measurement capabilities are expanded through development of experimental rig and loop systems where reactor fuel and material can be tested under light water reactor conditions, including prototypic PWR and BWR water chemistries.

Fuels testing at the Halden Project has for a number of years focused on implications of extended burnup operation schemes aimed at an improved fuel cycle economy. The extrapolation of low burnup experience to the envisaged exposure levels requires data for model development and validation. The experimental programmes are therefore aimed at identifying long term property changes. The data generated in the Halden Project fuels testing programmes originate from in-pile sensors which allow to assess

- fuel centre temperature and thermal property changes as function of burnup;
- fission gas release as function of power, operational mode and burnup;
- fuel swelling as affected by solid and gaseous fission products;
- pellet - cladding interaction manifested by axial and diametral deformations.

Investigations of fuel performance parameters, especially at high burnup, have to deal with a number of experimental problems, i.e. the time required for burnup accumulation, the demand on instrumentation to function reliably for the long time of in-core service, and the need for a separation of an increasing number of phenomena. The Halden Project has developed and applied techniques which make it possible to obtain reliable data for all relevant burnups, from beginning-of-life to ultra high exposure reaching 100 MWd/kgUO2. Among these are rod designs simulating high burnup effects such as closed gap and fill gas contamination with fission gas, accelerated burnup accumulation, and the re-instrumentation of pre-irradiated fuel segments [1, 2].

The irradiation of instrumented fuel rods can be carried out in specialised rigs according to test objectives, e.g. long term base irradiation, diameter measurements or ramps and overpower testing. The gas flow rig allows the exchange of fuel rod fill gas during operation which makes it possible to determine gas communication properties as well as the gap thermal resistance and its influence on fuel temperatures. It is also possible to analyse swept out fission products for assessment of structural changes and fission gas release. This is an important experimental technique for the high burnup programmes currently being executed.

The paper presents in-core data and findings on effects which may relate to the RIA behaviour of high burnup fuel. The results, which encompass both thermal and mechanical data from the Halden Project’s experimental programme, can be used to establish the starting conditions of a transient and may also be relevant for the phase during and shortly after the RIA energy deposition.
2. PELLET - CLADDING GAP

Standard fuel designs employ a gap between pellet and cladding of about 2% of the pellet diameter (150 - 250 \( \mu m \)) to accommodate thermal expansion and fuel swelling. A good knowledge of gap size and its change with burnup is essential for prediction of both thermal and mechanical behaviour. An underestimation of gap conductance at high burnup will lead to overprediction of fuel temperatures (stored energy) and fission gas release and may thus severely impact safety assessments.

**Gap closure due to fuel swelling**

The closing of the gap with burnup can be observed in many ways. A particular technique is the determination of the "hydraulic diameter" from the flow of gas (0.5 - 1.0 l/min) through the gap, driven by a pressure difference of 20 - 60 bar. Fig. 1 shows the development with burnup averaged for three rods of identical design (200 \( \mu m \) as fabricated diametral gap). After an initial drop, the decrease follows solid fission products fuel swelling. At high burnup, the gap seems to approach a minimum value, indicating that pellet-cladding contact restricts a further diameter increase; rather, the fuel may comply with closing cracks and creep. The gap at power is closed at about 30 kW/m at a burnup of 50 MWd/kg UO\(_2\).

![Figure 1: Gap closure determined with hydraulic diameter measurements. After an initial decrease due to pellet fragment relocation, the gap closes with fuel swelling.](image)

**Influence on thermal behaviour and change with burnup**

The determination of gap conductance at high burnup is affected with uncertainties which accumulate throughout irradiation. The problem can be alleviated by using - for experimental purposes - rod designs with small gaps (simulated gap closure) and Xe fill gas (simulated fission gas release). This has been applied in a large number of fuel tests at the Halden Project. A summary of the influence of gap size and fill gas type on fuel centre temperature determined in this way is shown in Fig. 2. The data represent the starting conditions where the gap size is not yet changed and uncertain due to fuel densification. As fabricated diametral gaps ranged from 50 to 230 \( \mu m \) for Xe filled rods and 50 to 400 \( \mu m \) for He filled rods.
It is of interest to note that the temperature difference between helium and xenon filled rods disappears at small initial gaps which are strongly closed at power. This implies that gap conductance is dominated by solid-solid contact conductance in this situation. Consequently, a gap conductance model should not retain a minimum separation between pellet and cladding due to roughness and waviness in the case of very hard contact. A similar conclusion was reached in [3, 4].

The improved gap conductance as a consequence of gap closure due to fuel swelling can also be observed in-pile as a function of burnup. An example is given in Fig. 3, again for a Xe filled rod where improved gap conductance supersedes fuel conductivity degradation, resulting in an overall temperature decrease after the initial densification phase.

**Fig. 2:** Typical fuel centre temperatures in He/Xe filled rods. Differences disappear with tightly closed gap.

**Fig. 3:** Fuel centre temperatures at 25 kW/m in a Xe filled rod with 100 μm as fabricated gap. The gap conductance improvement due to gap closure outweighs fuel conductivity degradation.
**Mechanical interaction**

Pellet-clad mechanical interaction (PCMI) can be measured in-pile in two ways: with a diameter gauge moving along the length of a rod, and with a cladding elongation detector. Since axial elongation can be measured more easily and frequently than diametral deformation, the data should not be neglected. However, the difficulties of modelling axial PCMI are recognised; they are probably the main reason why only few codes try to include the effect in a non-simplistic way.

Cladding elongation can also provide information on diametral deformation. The close relation between hoop strain and axial strain, both in terms of magnitude and relaxation behaviour, has been shown with Halden Project data comparing elongation and diameter changes obtained in-pile for the same rod [5].

The development of axial interaction with burnup for four similar rods (active length 768mm, pellet diameter 12.59mm, diametral gap 170μm) is shown in Fig. 4. It is a common experience that axial interaction during the first rise to power is strong and decreases during the following cycles. This can be attributed to random eccentric stacking of the pellets which are pushed to more central positions in contact with the cladding [6]. It should be noted that thermal expansion is not sufficient to close the gap at start-up (not even with fuel swelling at end of life, 34 MWd/kgUO₂), thus conventional models based on concentric geometry would not calculate any interaction, obviously at variance with the experimental evidence.

It is apparent that the onset of interaction (defined as point of deviation from free thermal expansion) moves to lower power with increasing burnup. There is also a change in curve shape to a more abrupt transition from free thermal expansion to expansion following fuel elongation. However, the interaction remained small in the examples because power in general did not exceed previously reached levels. As already discussed with the hydraulic diameter measurements, a kind of balance between fuel swelling and creep caused by contact forces seemed to have evolved. Since there is no gap left between fuel and cladding at power, a transient will lead to cladding load from the beginning.

**3. DEGRADATION OF UO₂ THERMAL CONDUCTIVITY**

Most fuel properties and phenomena are temperature dependent. An accurate description of the temperature distribution in a fuel rod is therefore required before other effects can be quantitatively defined. Conductivity degradation of UO₂ has been manifested both with simulated and in-reactor burnup [2, 7, 8, 9] and is now generally accepted as an important phenomenon to be considered in modelling of high burnup fuel behaviour. There is a general consensus that the effect is due to increased phonon scattering caused by the accumulation of fission products. The Halden Project's fuel testing programme contains a number of experiments where temperature measurements allow the conductivity degradation to be inferred. In general, increasing temperatures are observed in such tests, but the effect may be partly covered by improved gap conductance due to fuel swelling and gap closure (ref. Fig. 3). The temperature evolution in a dedicated test irradiated to very high burnup is shown in Fig. 5. The evaluation of this and other experiments points to a degradation factor of about b=3 (K·m/kW per MWd/kgUO₂) in the UO₂ conductivity term \( \lambda_{\text{phonon}} = (a + b \cdot \text{burnup} + c \cdot T)^{-1} \). It should be noted that the constant “b” also accounts for other irradiation dependent effects which may have an influence on conductivity, i.e.
CLADDING ELONGATION AT DIFFERENT BURNUPS

Burnup (MWd/kgUO$_2$): -1 0 -2 5 -3 15 -4 28 -5 34

Fig. 4: Cladding elongation of BWR fuel rods and change of interaction onset with burnup
Fig. 5: Measured temperature increase due to fuel thermal conductivity degradation

...microcracking, Frenkel defects and the formation of small fission gas bubbles. The constant "b" is therefore larger than obtained from out-of-pile tests with simulated burnup adding only solid fission products.

Together with gap conductance, the UO₂ thermal conductivity has an influence on the initial enthalpy (if the transient occurs at non-zero power) and the heat flow to the coolant after the energy deposition. Under normal (non-dryout) cooling conditions, the time constant of heat removal from the fuel can be obtained from the temperature decay following a reactor scram. An example is shown in Fig. 6 for a BWR type rod at three different burnups. An increase of the time constant due to conductivity degradation (no fission gas release) is apparent.

Fig. 6: Temperature decay of BWR fuel after scram
4. FISSION GAS RELEASE

The release of fission gas from UO$_2$ fuel continues to be a subject of considerable interest. At high burnup, the release may lead to rod overpressure and become a life-limiting factor. The influence on fuel temperatures and stored energy via gap conductance has direct consequences for the assessment of core reliability and safety during normal operation and transients.

A possible enhancement of fission gas release with burnup has been reported in several publications [10, 11], but evidence for behaviour more "as expected" to burnups up to 50 MWd/kg UO$_2$ has also been presented [2, 12]. An enhancement can be associated with two effects: the formation of a porous rim with increasing athermal release, and higher fuel temperatures due to poorer conductivity of the rim as well as a general UO$_2$ conductivity degradation.

Fission gas release model

Fission gas release has been investigated extensively at the Halden Project using rods instrumented with pressure transducers and fuel centre thermocouples. A well known result from these studies is the discovery of a temperature threshold for the onset of appreciable release (> 1%), [13]. The original empirical correlation covered burnups to 30 MWd/kg UO$_2$ and could later be explained with the discovery of bubbles on the grain boundaries (e.g. [14]) and their interlinkage to a tunnel network providing a release path to the open surface (the structural change is apparent as an increase of the fuel surface-to-volume ratio measured in gas flow experiments [7]). Using a Booth diffusion model together with a storage of gas at the grain boundaries up to a concentration limit of $5 \times 10^{15}$/cm$^2$ before release occurs, the empirical threshold could be well reproduced (for details of the model see [15]).

The release threshold rule is quite accurate as can be seen in the example shown in Fig. 7. The fuel rod was of short length (14 cm) with a thermocouple inserted such that a good knowledge of fuel temperature was available for the entire length. Release onset can be seen with its effect on temperature at around 27 MWd/kg UO$_2$ when the threshold was reached or exceeded for the first time. The temperature data reflect the gap contamination with fission gas in two respects: a) a temperature increase as seen from the curve of data normalised to a constant power of 25 kW/m, and b) a change of the shape of temperature-versus-power curves from slightly positive to negative curvature which is a characteristic difference between He and Xe filled rods. (This can also be used to distinguish between temperature increase due to conductivity degradation and due to fission gas release. The increase up to 27 MWd/kg UO$_2$ can be explained with the degradation coefficient indicated above.)

Release enhancement

The release model takes into account known effects with an influence on fuel temperatures such as conductivity degradation and gap closure, either by directly using measured temperatures or by applying best estimate correlations from the Halden Project's experimental programme. A release enhancement may therefore be defined as due to effects not accounted for by the model and apparent as a definite deviation from predictions.
Fig. 7: Fission gas release after exceeding the release threshold temperature. Temperatures increase due to changed gap conductance.
The model described above has been applied to many experiments from the Halden reactor and gives good agreement with measurements especially when temperatures are known (the fuel temperature is otherwise a major source of uncertainty due to the exponential dependence of the diffusion coefficient). An example is shown in Fig. 8; it can be seen that fission gas release as inferred from pressure measurements is well followed by the model to a burnup of 56 MWd/kg UO$_2$. During the last part of irradiation, a deviation becomes apparent which may be due to an effect not accounted for by the model. Although a pronounced rim development does not occur in the Halden reactor, it should be noted that the peak burnup (axial, radial) measured in MWd/kgU has reached about 70 at the point of deviation. This number is now regarded as the lower limit for rim structure formation.

Another application of the release model is shown in Fig. 9. Fission gas release (about 2%) is calculated for a period with high temperatures early in life, followed by little further release as also shown by the pressure measurements. A definite change of slope of the pressure versus burnup curve occurs at about 58 MWd/kg UO$_2$. The release continues despite decreasing temperatures during the last part of irradiation.

The total release as deduced from the pressure change is still small in this case (about 4%), and an appreciable amount of fission gas must be stored on the grain boundaries. Especially at lower temperatures, the gas atoms will remain segregated and lead to a grain boundary embrittlement. It can be expected that this has a bearing on transient fuel behaviour. The same may be true for not yet interlinked bubbles on the grain boundaries.

**Burst release during power decrease**

Steps of pressure increase coinciding with reactor shut-downs can be noted in the high burnup part of Fig. 10, see indication at 65 MWd/kgUO$_2$. This is detailed in Fig. 10 where pressure is shown as function of rod power for a period of start-up, 12 days at power and shut-down. The entire pressure increase occurs during power decrease and is finished at 10 kW/m. This behaviour has also been observed in other experiments and is associated with the propensity of the fuel to release fission gas together with a small residual gap due to fuel swelling and/or thermal expansion. Both effects increase with increasing burnup.

It can be assumed that the gas was released to the interlinkage network of gain boundary bubbles during the steady state period, but could not escape to the gap and the plenum. With decreasing power, old and new cracks open and allow the gas to escape. This accumulation of fission gas already at high pressure may have an influence on transient behaviour (increased mechanical load) when a transient starts from high power.
Fig. 9: Pressure increase due to fuel swelling and fission gas release in a rod irradiated to high burnup. Model predictions deviate more pronounced for exposure exceeding 58 MWd/kgUO₂.

Fig. 10: Burst release of trapped fission gas during power decrease at 65 MWd/kgUO₂.
5. GAS TRANSPORT THROUGH THE GAP

The data on hydraulic diameter measurements and cladding elongation presented in the previous sections have given evidence for a closed gap at high burnup. Gas transport through the gap is severely restricted in this case. Pressure equilibration experiments have shown that flow driven by a pressure difference of 30 bar may be as low as 60 cm$^3$/h, decreasing approximately with $\Delta P^2$. Gas retained in cracks (the pressure step in Fig. 10 corresponds to 2.5 cm$^3$ STP) or released during a transient will therefore hardly reach the plenum, but rather contribute to loading the cladding. While work produced by solid material (fuel) thermal expansion is limited by a small change of geometry, expanding gas can eventually be more disruptive.

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Tensile Properties of Irradiated Zircaloy 4 Cladding Submitted to Fast Transient Loading

M. Balourdet¹, C. Bernaudat²

Abstract

Detailed knowledge of transient mechanical properties of irradiated Zircaloy 4 cladding is needed for the modelling, by the IPSN in Cadarache, of Reactivity Initiated Accidents of PWRs and for the interpretation of RIA type, power burst tests performed in CABRI and NSRR. Loading conditions envisaged during RIAs are quick heating up rates (of the order of $10^3$ K.s$^{-1}$), fast strain rates (of the order of 1 s$^{-1}$) and temperatures between 280 and 1482°C. The material of interest is stress relieved Zircaloy 4 cladding irradiated up to the new target burnup of EDF commercial reactors.

At the CEA in Saclay, samples from 59 GWd/t of irradiated Gravelines rods, as well as unirradiated ones, have been tensile tested in the rolling direction after either rapid (100 K.s$^{-1}$) or slow (ca. 0.3 K.s$^{-1}$) heating up to the test temperature (400 to 1100°C); three strain rate values have been applied (0.01, 0.2 and 5 s$^{-1}$). On unirradiated samples, the yield stress and the ultimate tensile strength are observed to increase with increasing strain rate without any marked influence of the heating conditions, but uniform elongations obtained after slow heating are much higher near 600 to 700°C due to some recrystallization. The YS and the UTS of the irradiated cladding are close to those of the fresh material. The UE is more or less reduced with respect to the unirradiated cladding but it is still positive; similarly enhanced elongations are obtained after slow heating.

The likelihood of early loading of a cold cladding during RIAs and the potential of a severe embrittlement due to hydride accumulations have been demonstrated by the first RIA test in CABRI. Therefore, a greater emphasis shall be put on lower temperatures (280 to 600°C) and on loading along the transverse direction with a further series of transient tensile tests at the CEA in Saclay. Also, a special programme has been launched at the CEA in Grenoble in order to investigate the brittle-ductile transition between 300 and 400°C as a function of the strain rate.

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Introduction: needs

In view of the new target burnup of EDF PWRs, the transient behaviour of high burnup fuel rods during a Reactivity Induced Accident (RIA) has to be assessed (when the CABRI REP programme had been formulated, the confirmed experimental data base on rod failure was limited to a 32 GWD/tU maximum burnup). A computer code, SCANAIR has been developed by the IPSN in Cadarache for the modelling of these accidents and an experimental programme has been launched by the IPSN with RIA type power burst tests in the CABRI reactor [1]. Studies are based on the interpretation of such tests [2], as well as of similar ones, carried out by JAERI in NSRR. For these, detailed knowledge of transient mechanical properties of irradiated FRAGEMA cladding used by EDF (stress relieved Zircaloy 4) is needed: they should allow the transient response of the cladding to be calculated and a failure criterion to be established.

At the beginning of the transient, the cladding should be loaded very quickly by pellet-clad mechanical interaction (PCMI) due to fuel expansion or swelling and in a second phase, if failure has not occurred already, slower loading by the pressure of released gases can take place. Power ramps envisaged are rather fast (ca. 0.1 s half width in the reactor case and 0.01 s for most experiments) so that high strain rates (of the order of \(1 \text{s}^{-1}\)) can be expected during the first phase especially, as it has been proposed by the IPSN for the interpretation of the CABRI REP-Na 1 test, if early pressurization takes place because of quick availability of fission gases retained in the rim [3]. Indeed, such high strain rates have been observed with NSRR and CABRI tests. Also, the cladding is heated up rapidly (at ca. \(10^3 \text{K.s}^{-1}\)). In principle, temperatures of interest cover a wide range, from 280 to 1482°C. Currently available data (tensile & burst tests) from PWR surveillance programmes are limited to slow strain rates (\(3x10^{-4} \text{s}^{-1}\)) and to nominal temperatures (25 & 350°C): thus, they do not match the rapid loading conditions encountered during the first phase of RIAs; only the modelling of the second phase can make use of available creep data. Therefore, mechanical testing programmes had to be set up in order to provide input data for accident codes such as SCANAIR.

The information sought after is the stress-strain relationship in a tensile test as a function of the main parameters: pre-test condition of the cladding after in-pile irradiation, transient thermal history, test temperature, loading direction and strain rate. The first factor includes the radiation damage, roughly proportional to the local burnup, and the corrosion, i.e. the degree of hydriding, which varies significantly along a single rod and between rods too; also, some mechanical damage, for instance because of load follow operation, may have altered the initial state. However, testing had to be restricted to a limited range of these parameters.
Hot cell techniques and preliminary tests

Profit has been taken from the experience gained by the CEA in Saclay on high strain rate tensile tests, after thermal transient, of two wing samples machined, after defueling, from LMFBR irradiated stainless steel cladding [4]. Strain rates up to 50 s⁻¹ and heating rates in excess of 1500 K.s⁻¹ had been achieved. The sample shape, obtained in a hot cell by spark machining of 6 cm long defueled clad tubes, ensures that heating is concentrated in the wings and that the plastic strain is limited to a gauge length of 5 mm (Fig.1) The desired transient heating is provided by an electric current passing through the sample, the temperature being measured by a thermocouple welded on one of both wings; once the target temperature is reached, it is maintained constant and the tensile test is triggered at a fixed strain rate. Tests are carried out in the hot cell under air atmosphere. The same technique can be used with defueled PWR cladding; however, ZrO₂ layers must be removed mechanically from the Zircaloy before spark machining.

Prior to the main programme, the feasibility has been checked in 1991 with 12 transient tensile tests between 650 and 1250°C on samples machined from unirradiated Zircaloy 4 tube [5]; parameters investigated were the strain rate with two values (0.01 & 5 s⁻¹) and the heating rate with two values (ca. 100 K.s⁻¹ & more than 500 K.s⁻¹). It appeared that results were insensitive to the heating rate in the range considered but the strain rate had a marked influence; lower yield stresses (YS) and lower ultimate tensile strengths (UTS) were obtained with the slower rates (Fig.2). Above ca. 1000°C, stresses are too low (less than 20 MPa) to be measured with accuracy; also, the samples suffer from oxidizing by the air. Variations with temperature are consistent with correlations already used by codes.
Scope of the first programme

Conditions addressed are rapid transient heating and fast loading. As a result of the feasibility tests, only one thermal transient can be considered: a 100 K s\(^{-1}\) heating rate has been chosen as it allows a better temperature control. In order to ascertain the influence of the strain rate, three values have been retained: 0.01, 0.2 & 5 s\(^{-1}\) (the latter value corresponds to the initial phase with PCMI loading whereas the former one can be expected during latest stages in the course of the transient).

According to pre-calculations of RIAs, PCMI loading was not expected to start too early, i.e. not until a 600°C clad temperature had been reached. Under such conditions, embrittlement of irradiated Zircaloy by hydrides should be minimal. Also, mechanical anisotropy should prevail only at temperatures below those where a significant loading is predicted. On the other hand, it was not deemed worthwhile to measure very small strengths for temperatures above 1100°C. Therefore, the temperature range of interest has been limited to 400 up to 1100°C.

In view of the temperature range of main interest, it was not felt necessary at this time to investigate the anisotropy: therefore, only longitudinal tensile tests, on samples machined along the rolling direction, were devised. Also, they provide data (YS, UTS, UE) which can be put readily into codes whereas results from tests, along the transverse direction, on ring samples need some treatment (not available at that time) before being of any use.
The influence of the post-irradiation state should be thoroughly investigated. However, only two conditions have been considered: irradiated cladding taken from 4 fuel rods with 59 GWd/t burnup (5 runs in the Gravelines reactor) and unirradiated regular FRAGEMA clad tubes from similar batches to serve as a reference. Only sparse locations, with ZrO2 layers ranging between 20 and 70 µm, were available on these rods so that, due to the limited number of samples (26) with regard to the parameters of interest, the influence of corrosion could not be investigated in detail: all samples are assumed to be characterized by an average ZrO2 thickness of 50 µm.

At the beginning of the programme on irradiated Zircaloy, some difficulties have been encountered upon spark machining the samples with a 5 mm gauge length as desired. Therefore, about one half of the samples had to be machined with the current geometry used for tensile tests of the surveillance programme: the gauge length is longer, 15 mm which ensures a better accuracy on the elongations, but no thermocouple can be adapted. These samples are only intended for furnace heating, i.e. with slow heating rates (ca. 0.3 K.s⁻¹). In order to minimise unwanted consequences of pre-test annealing for both fresh and irradiated cladding, only lower temperature tests (400 to 800°C) have been carried out under these conditions while actually transient heating has been employed in the higher temperature range (700 to 1100°C).

Longitudinal tensile tests with fresh cladding

Conventional results are summarized on table 1.

Fig.3: Transient tensile tests with unirradiated cladding

0.2% Yield Stress (MPa)

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<th>0.2 s⁻¹ - furnace heated</th>
<th>5.0 s⁻¹ - furnace heated</th>
<th>0.01 s⁻¹ - transient heated</th>
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421
As observed with the previous 12 tests, the yield stress (Fig. 3) and the ultimate tensile strength (Fig. 4) of the unirradiated 26 samples increase with increasing strain rate, especially between 0.2 and 5 s⁻¹, with $0.1 < \frac{\partial \log UTS}{\partial \log \dot{\varepsilon}} < 0.2$. On the average, the strengths are higher than those measured with the feasibility tests but these had been done.
with a different batch. There is no marked difference between results from Joule and furnace heating conditions [6, 7].

**Fig.6: Transient tensile tests with unirradiated cladding**

*Total Elongation (%)*

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Uniform elongations (Fig.5) measured after rapid heating decrease slowly with increasing temperature without any pronounced effect of the strain rate. Those obtained after slow heating are much higher near 600 to 700°C. After slow heating, total elongations too (Fig.6) have a marked maximum in the same temperature range; those measured after transient heating are of dubious value because, in contrast with furnace heated samples, temperatures in the wings may not be homogeneous after necking onset: they are not shown here.

**Longitudinal tensile tests with irradiated cladding**

The 26 samples have been taken from 4 rods irradiated to a burnup of ca. 59 GWd/tU: it is only slightly lower than that (63 GWd/tU) of the QO2 rod tested in CABRI with REP-Na 1 but, due to different irradiation conditions (in particular, the fraction of load follow operation, only one run, was less important than for QO2) and maybe because of different Zircaloy batches, the cladding is less corroded than in QO2 and, above all, it does not suffer from spallation. The degree of pre-test corrosion on the parent rods can be crudely characterized by the ZrO₂ thickness measured at the corresponding location on a particular rod (n° 1066); a majority of samples stems from fourth spans, i.e. with ca. 50 μm ZrO₂.

Conventional results, based on the actual sample dimensions measured after removal of the oxide, are summarized on table 2.
Fig. 7: Transient tensile tests with irradiated cladding
0.2% Yield Stress (MPa)

Fig. 8: Transient tensile tests with irradiated cladding
Ultimate Tensile Strength (MPa)

The YS (Fig. 7) and the UTS (Fig. 8) of the irradiated cladding are close to those of the fresh material, displaying, on the average, similar variations with temperature and strain rate, and, apparently, no significant influence of the heating rate [7, 8]. It cannot be decided whether discrepancies with the average trends must be attributed to differences in the pre-test corrosion.
On the average, the UE (Fig.9) is more or less reduced with respect to the unirradiated cladding but it is still positive; also, it must be noted that, due to the sample geometry which leads to some flexion, deformations are underestimated. Similarly enhanced elongations are obtained after slow heating. Again, no particular influence of the degree of corrosion can be discerned: the conclusion might be different if the number of samples were less limited; however, much more numerous...
values, measured for the surveillance programme, are even more
dispersed without any clear correlation with the sample position, i.e. with
the corrosion.

The total elongation (Fig.10) displays similar variations compared
with the unirradiated cladding. Also, higher values, with respect to tests
after rapid heating, are found near 700°C after slow heating.

Metallographic examinations

In order to understand the enhanced ductilities obtained near 700°C
after slower heating, 5 samples, furnace heated and tensile tested
between 480 & 780°C at 0.01 s⁻¹ strain rate, have been examined by
optical micrography [9]. Incipient recrystallization at 700°C is evidenced; it
is complete at 780°C. Also, some extra oxidizing, occurring during the
furnace heating, has been observed (up to 10 μm at 780°C).

Lessons from CABRI experiments

Various PWR rods have been submitted to fast power ramps (half
width ca. 10 ms) in CABRI [1]. The first test, REP-Na 1 with span 5 from a
63 GWD/ft² rod (Q02), led to an unexpectedly premature failure when the
pellet averaged enthalpy at peak power position was only 30 cal/g
(12 cal/g injected energy) and with a clad temperature only slightly higher
than the pre-pulse value of 280°C. Thus, it became obvious that stresses
may develop early, i.e. even within a cold cladding, and that mechanical
properties measured at higher temperatures could not help to the
interpretation of this particular case.

On the other hand, another test, REP-Na 5 with span 2 from another
63 GWD/ft² rod, ended without any failure in spite of 115 cal/g injected
energy. In this case, the cladding was much less corroded (20 μm ZrO₂
layer instead of 80 μm for span 5 of Q02) and without any spallation
whereas the oxide layer in Q02 was heavily scaled off. This underlines the
importance, for the transient behaviour, of the clad condition, at a given
burnup, resulting from the pre-irradiation.

Post-test examinations of REP-Na 1 gave evidence of brittle type
failure with longitudinal crack propagation. Also, many, sunburst like,
features have been observed, by metallography, towards the clad outer
surface and identified as local accumulations of hydrides formed during
the pre-irradiation at cold spots due to prior spallation of the outer
oxide [10]: such features, with a much greater hydride content than the
bulk (ca. 700 ppm mean hydrogen concentration) of the cladding must be
responsible for an enhanced embrittlement, especially under fast loading
at a low temperature. Indeed, using sodium flow expulsions measured
during non-failure tests (e.g. REP-Na 2), strain rates can be estimated to
be of the order of 5 s⁻¹.
Even slow rate \((3 \times 10^{-4} \text{ s}^{-1})\) mechanical tests of the surveillance programme at not too low a temperature \((350^\circ \text{C})\) may lead us to suspect hydride induced embrittlement and the presence of local heterogeneities: results, especially the ductilities \((\text{UE} \sim 3\%, \text{ TE} \sim 6\%)\), of tensile tests in the rolling direction of 5 runs irradiated cladding are quite consistent with those obtained under RIA conditions at 400°C. However, burst tests on 120 mm long clad samples resulted in strongly reduced uniform elongations, i.e. five times smaller (even zero for a few, dubious, tests); also, the elongations are more scattered than those from tensile tests. Similar results are obtained to a lesser extent after 3 and 4 runs. Tensile tests in the transverse direction do not end in so pronounced ductility reductions; therefore, randomly distributed defects, such as local hydride accumulations, may be responsible for an embrittlement which is more prominent with burst samples. The situation could only be worse with the REP-Na 1 cladding as it was particularly corroded.

**Conclusion: further programmes**

In view of the CABRI REP-Na 1 result and of the information from the surveillance programme, it appears that a greater emphasis must be put on lower temperatures, also with the objective to interpret NSRR tests. It must be reminded that the, highly corroded, 5 run Gravelines rods considered belong to an old fabrication which is not representative of rods presently loaded in EDF reactors. Thus, another matter of interest is the new FRAGEMA cladding, with tighter specifications and improved corrosion resistance (AFA 2G).

Therefore, in order to provide input data for the codes, a second programme has been launched at the CEA in Saclay: it is aimed at supplementing, towards the lower temperatures, the already existing data set; also, with these low temperatures, the anisotropy must be investigated. Thus, in 1995 and 1996, high strain rate tensile tests (at 0.01 & 5 s\(^{-1}\)) will be performed between 280 & 600°C in the rolling and transverse directions (respectively 24 and 48 tests) of irradiated cladding. Transverse tensile tests use ring samples, with two thinner portions at opposite angles, loaded through hemicylindrical mandrels; with such a geometry, sample flexion is significant and the raw results have to be corrected by computer treatment. In addition, two rapid (ca. 0.01 s\(^{-1}\)) burst tests are to be performed at 350°C with samples from span 6 of a 5 run rod: the consistency with uniaxial tensile tests will be checked and, also, the results will be compared with slower burst tests of the surveillance programme.

The clad corrosion is characterized by eddy current measurement of the oxide thickness along the rods to be cut and defueled. In order to study the influence of corrosion, samples have been selected from various positions, either with small or with important oxide thicknesses, within spans 2 to 4 of four, 5 run irradiated, rods with standard cladding and along a 4 run irradiated rod with improved cladding (AFA 2G). The samples are preferably taken from regions relatively less affected by spallation so that results should depend mainly on the bulk hydrogen
content; local effects shall be investigated through another programme in Grenoble.

Hydride induced embrittlement is already known from out-of-pile experiments: for instance, impact tests on Charpy V-notched specimens of gaseously hydrided Zircaloy 2 have shown that a 700 ppm hydrogen concentration resulted in brittleness below 380°C while a 270°C transition temperature can be estimated from slower bend tests [11]. At low temperatures near 300°C, irradiated cladding, with up to 700 ppm mean hydrogen concentration, can be expected to be more severely embrittled, especially under fast power transients. Also, consequences of local accumulations of hydrides, presumably linked with spallation, should be studied.

Therefore, a special programme has been launched at the CEA in Grenoble in order to investigate the brittle-ductile transition between ca. 300 & 400°C as a function of the strain rate in the range 10^{-4} to 40 s^{-1}. The relevant parameter is the total elongation measured during tensile tests performed in the transverse direction on ring samples taken within span 4 and span 6 of 5 run irradiated rods (M05 & Q02), span 5 of Q02 having been used for CABRI REP-Na1: here, regions with important spallation have been selected in order to investigate local effects. The hydrogen content in many of the samples will be measured after tensile testing.

Acknowledgements

The authors wish to thank J. ROYER and his colleagues of the CEA in Saclay for the performance of the mechanical testing and for their expert advice.

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Zircaloy"
Journal of Nuclear Materials 22 (1967) 137-147
Tab.1: Tensile tests with unirradiated cladding

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<th>Strain rate (s⁻¹)</th>
<th>Temperature (°C)</th>
<th>0.2% YS (MPa)</th>
<th>UTS (MPa)</th>
<th>UE (%)</th>
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Tab. 2: Tensile tests with irradiated cladding

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<th>Strain rate (s⁻¹)</th>
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after furnace heating (15 mm gauge length)

after transient heating (5 mm gauge length)
Influence of locally concentrated hydrides on ductility of Zircaloy-4

Fumihisa NAGASE, Kiyomi ISHIJIMA and Teruo FURUTA

Department of Reactor Safety Research
JAERI

Abstract

With a view to investigate the influence of locally concentrated hydrides on ductility of Zircaloy-4 cladding, laboratory experiments were performed. Zircaloy-4 plate specimens containing 300 to 800 wt ppm H and having hydrogen concentration gradients in the thickness or width direction were tensile tested at room temperature and 623K. Ductilities of specimens differed depending on hydride distribution and test temperature. At room temperature, specimens with strong hydrogen concentration gradient had slightly higher total elongation than those with uniform hydrogen distribution, which exhibited obvious hydrogen embrittlement. In the former specimen, slower crack propagation was observed in less hydrided part. At 623K, specimens with uniform hydrogen distribution exhibited improved ductility, while specimens with strong hydrogen concentration gradient showed relatively smaller total elongation due to preferential crack formation at the highly hydrided part even at this temperature.

1. Introduction

The amount of hydrogen absorbed in the Zircaloy cladding increases with burnup extension in LWRs. Hydrogen embrittlement is one factor limiting the life of fuel rods. Locally concentrated hydrides are often found in the region near the outer surface of the cladding. A temperature gradient is present in the cladding thickness during operation and the outer surface is relatively cooler. Localization of hydrides is mainly caused by the temperature gradient. Formation of the highly hydrided part may have an effect on ductility of the cladding under the conditions of normal operation, the transient and RIA. In the pulse irradiation experiment \(^{(1)}\) in the NSRR, a high burnup PWR fuel rod failed at a very low energy deposition level. The PIE on the failed rod revealed
localization of hydrides in the outer surface region of the cladding. Therefore, the mechanism of the failure is considered to be strongly related with the mechanical interaction between the pellet and the highly embrittled cladding. This paper describes the results of the fundamental experiments that were performed to investigate the influence of locally concentrated hydrides on ductility of Zircaloy-4.

2. Hydrogen distribution under temperature gradient

The binary phase diagram of zirconium-hydrogen system is shown in Fig.1(2). The terminal solid solubility of hydrogen decreases rapidly with decreasing temperature below 700K and it is about 120ppm at 623K(3). When the hydrogen content exceeds the terminal solid solubility, δ hydride precipitates in α phase. It is also known that hydrogen moves toward a cooler region under the influence of a temperature gradient. Therefore, δ hydride precipitates preferentially at the cooler side in zirconium having a temperature gradient. Once the (α+δ) region is formed, the hydrogen concentration of the (α+δ) region increases with time by diffusion of hydrogen from the hotter side. After a certain time, the region containing so concentrated hydrides is formed at the cooler side(4,5).

Assuming that hydrogen is the only component diffusing, that hydride is in equilibrium with solid solution at every point in the (α+δ) region and that diffusion occurs predominantly in the α region, the hydrogen concentration in the (α+δ) region Na+δ is given approximately by

\[ N_{\alpha+\delta}(T) = N_i + \left[ \frac{D_0 N_0}{(RT)^4} \right] \left( \frac{\Delta H+Q^*}{R} \right) (\frac{dT}{dx}) \times t \left\{ \frac{(\Delta H+Q)}{R-2T} \right\} \exp \left\{ - \frac{(\Delta H+Q)}{RT} \right\} \]

where \( N_i \) is the initial hydrogen concentration, \( D_0 \) the frequency factor, \( N_0 \) a constant, \( \Delta H \) the heat of mixing, \( Q \) the activation energy for diffusion, \( Q^* \) the heat of transport, \( t \) the annealing time,

\[ \text{Fig.1 Zr-H phase diagram} \]
and $dT/dx$ the temperature gradient. The hydrogen concentration in the $\alpha$ phase at the hotter side is given by the simple Arrhenius equation.

Figure 2 shows a temperature gradient in the cladding thickness of a high burnup fuel. The linear heat rate $q'$ became half of that at the beginning of the life and $50\mu$m of metal thickness was oxidized here. The temperature difference and the gradient were calculated to be 24K and 41.4K/mm, respectively. Hydrogen distribution under this temperature gradient was calculated assuming that the sample initially had uniform hydrogen distribution of 250ppm H and that it was annealed for 30 days. No hydrogen absorption was considered during annealing. Calculated hydrogen distribution is shown in Fig.3. Most of the contained hydrogen was found to be concentrated in the limited thickness near the outer surface. Thickness of the hydrogen concentrated region was 2 to 3 percent of the whole thickness that corresponded to the temperature range less than 1K. Hydrogen is in solid solution in 90 percent of the thickness. Thickness of the $(\alpha+\delta)$ region becomes broader at a lower cladding temperature and under a smaller temperature gradient. A high burnup fuel has a lower linear heat rate compared with a lower burnup fuel, so that a thicker $(\alpha+\delta)$ region can be formed in the former case.

Actual distribution of hydrogen in the fuel cladding is expected to be different in detail, since the conditions were simplified in
calculation. Hydrogen absorption from the cooler side during operation, stress or other elements diffusing may have an effect on distribution of hydrogen.

### 3. Experimental procedures

Calculation shown above suggested that the local concentration of hydrogen can occur in the high burnup fuel cladding. Then, laboratory experiments were performed to investigate the influence of hydrogen distribution on ductility of Zircaloy-4. Tensile specimens with gauge section of 12.6×6mm were cut from recrystallized Zircaloy-4 plate (1.2mm thick) such that the tensile axis was parallel to the rolling direction of the plate. Hydrogenation was performed in about 0.3atm H$_2$ gas at 583K to achieve the nominal levels of 300 to 800ppm H. Hydrogen absorption was monitored by gas pressure during hydrogenation. Hydrogen concentration was determined by weight gain and hydrogen analysis consisting of extraction at high temperature and analysis by gas chromatography.

Two types of hydrided specimens having hydrogen concentration gradient were produced. The hydriding method is schematically shown in Fig.4. Before hydrogenation, specimens were oxidized in steam at 823K for 10h to form thin oxide film of 2 to 3μm thick and oxide was removed partially. Since thin oxide layer delays hydrogen absorption$^6$, hydrogen was absorbed preferentially from the metal surface. Oxide was removed from one top surface in Type A specimen and from the region near one side in Type B specimen. As a result, gradients of hydrogen concentrations were formed in a thickness direction for Type A and in a width direction for Type B. Figure 5 shows the hydrogen distributions of Type A containing 350ppm H and Type B containing 300ppm H.

![Fig.4 Schematic illustration of hydriding method for specimen with hydrogen concentration gradient](image_url)
Fig. 5  Hydrogen distribution measured with IMA; (a) thickness direction in Type A (350 ppm H) and (b) width direction in Type B (300 ppm H)

Fig. 6  Hydrogen morphology of Type B specimen with hydrogen concentration gradient in width (Average concentration: 300 ppm H)
Measurement was performed in the cross section perpendicular to the tensile axis with an ion micro analyzer (IMA). Highly hydrided region of this Type A specimen had 485 ppm H and the concentration was 1.5 times as high as that of the less hydrided region. In type B specimen, the difference of hydrogen concentrations was greater and it was 2.3 times. Figure 6 shows the hydrogen morphology at both ends of type B specimen containing an average hydrogen concentration of 300 ppm. Precipitation of larger hydrides is seen in the highly hydrided part (left).

Tensile tests were performed at room temperature and 623 K with the strain rate of $2.78 \times 10^{-7}$/s for most of the specimens and $1.39 \times 10^{-7}$/s for several specimens (only at room temperature). It was ascertained that oxidation at 823 K for 10h had no influence on the tensile properties.

4. Test results

4.1 Tensile test at room temperature

Figure 7 shows load-elongation curves for as-received and hydrided specimens with different hydride distributions tested at room temperature. The average hydrogen concentration was about 800 ppm. The specimen with uniform hydride distribution had the lowest ductility. Small cracks were formed in the early stage of the plastic deformation and instant crack propagation occurred at failure in this specimen. On the other hand, specimens with hydrogen concentration gradient

![Load-elongation curves for as-received and hydrided specimens with different hydride distributions tested at room temperature](image-url)
exhibited relative higher total elongations. Crack formation in highly hydrided region and slower crack propagation in less hydrided region were visually observed in Type B specimen. Such delay of crack penetration might result in slightly higher total elongations. Type B specimen exhibited a lower elongation than Type A specimen.

Effect of hydrogen concentration on total elongations at room temperature is summarized in Fig. 8. Ductility of the specimen with uniform hydride distribution was reduced as the hydrogen concentration increased. Its total elongation was about 45% at 0ppm H and it decreased to 20% at 800ppm H. Specimens with hydrogen concentration gradient had higher total elongations than those with uniform hydrogen concentration in the examined range of hydrogen concentration. That tendency was obvious in lower levels of hydrogen concentration. Elongations of samples became comparable at higher hydrogen concentrations, since less hydrided part of Type A or Type B specimen was also hydrided to such a high level that it exhibited embrittlement.

![Fig. 8](image)

**Fig. 8** Effect of hydrogen concentration on total elongations at room temperature

4.2 Tensile test at 623K

Figure 9 shows load-elongation curves for as-received and hydrided specimens with different hydride distributions tested at 623K. The average hydrogen concentration in the specimen was 700 to 760ppm. Ductility of hydrided Zircaloy is improved by higher ductility of matrix at higher temperatures. In the present study, elongation of the specimen with uniform hydride distribution and containing 700ppm H was comparable with that of unhydrided one at 623K. This improvement of ductility agrees with the previous study by Bai et al.

On the other hand, reduction of ductility was observed in specimens with hydrogen concentration
Fig. 9  Load-elongation curves for as-received and hydrided specimens with different hydride distributions tested at 623K

Test temperature: 350°C
Hydrogen Content: 760ppm

Fig. 10  SEM photographs of fractured surface near highly hydrided part of Type B specimen (Average concentration: 760ppm)
gradient at this temperature. As obviously seen in the curve of Type B specimen, decrease of load
started at small plastic strains due to the preferential crack formation at the highly hydried region.
Hydrogen concentration of that region is well above 1000ppm and improvement of ductility
cannot be expected up to a high temperature. Type A specimen also showed reduction of total
elongation, however, the influence of locally concentrated hydrides was smaller than Type B.

Figure 10 shows SEM photograph of the fractured surface near highly hydried part of Type
B specimen which had an average hydrogen concentration of 760ppm and was tested at 623K. Brittle
surface could be observed in the highly hydried region (indicated with arrows). Magnified
photograph A shows ductile surface of less hydried area, and B shows the brittle and the
transitional areas.

Effect of hydrogen content on total elongations at 623K is summarized in Fig.11. Two types
of specimens with hydrogen concentration gradient had always smaller total elongations than those
of specimen with uniform hydride distributions in the examined range of hydrogen concentration.
The influence of localized hydrides became greater at higher hydrogen concentration. Total
elongation of Type A specimen which exhibited the greatest reduction of ductility was about 70%
that of the specimen with uniform hydrogen distribution at 800ppm H.

![Fig. 11 Effect of hydrogen concentration on total elongations at 623K](image)

### 4.3 Tensile test at higher strain rate

Results of tensile tests at higher strain rate, \( \dot{\varepsilon}=1.39\times10^{-1}/s \), are summarized in fig.12. Tests were
performed at room temperature. Test results at smaller strain rate, \( 2.78\times10^{-4}/s \), are also shown for
comparison. There was a decrease of about 30% in total elongation of unhydrided specimen by
increasing the strain rate. Specimens with hydrogen concentration gradient also exhibited
reduction of ductility, while elongation of the specimen with uniform hydrogen distribution of 200 to 400ppm were independent of the strain rate. Therefore, it can be said that locally concentrated hydrides reduced more ductility in this comparison. Above 500ppm, specimen with uniform hydride distribution exhibited very low ductility.

![Graph](https://via.placeholder.com/150)

Fig. 12 Effect of hydrogen content and strain rate on total elongation

5. Summary

Ductility of hydrided Zircaloy-4 plate specimens differed depending on hydride distribution and test temperature. At room temperature, the specimen with hydrogen concentration gradient had slightly higher total elongation than the one with uniform hydrogen distribution. Slower crack propagation was observed in the less hydrided region of the former specimen. At 623K, the specimen with uniform hydrogen distribution exhibited improved ductility. On the other hand, the specimen with hydrogen concentration gradient showed smaller total elongation due to preferential crack formation at the highly hydrided region even at this temperature.

Locally concentrated hydrides reduced the ductility of Zircaloy more than uniformly distributed hydrides at a higher strain rate.

Several factors should be considered to evaluate the ductility of Zircaloy containing locally concentrated hydrides. They are the extent of hydride localization, deformation temperature and brittle to ductile transition temperature, sample geometry, texture of material, and strain rate.
References

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(2) Beck, R.L., Trans.ASM 55(1962)542
(3) Kearns, J.J., J.Nucl.Mater. 22(1967)292
(4) Schroymon, P.G., Trans.Met.Soc.AIME212(1958)642
(7) Huang, J.H. and Huang, S.-P., J.Nucl.Mater. 208(1994)166
EDF wants to increase the burnup discharge limit of its nuclear fuel assemblies. It is involved, together with CEA/IPSN in an important R&D program in order to demonstrate that the RIA (reactivity initiated accident) criteria are fulfilled for high burnup fuel. This program is centered on full-scale RIA tests in the CABRI reactor. But other studies, like the present one, are necessary for better understand the fuel rod behaviour during a RIA and to extrapolate the CABRI tests results to PWR conditions.

In this study, a pellet and its corresponding cladding part have been modelled by means of a 2D axisymmetric meshing, with EDF's Finite Element code ASTER. The pellet rim region, which is modelled with a 3D meshing, is represented in the global 2D-model with an equivalent homogenized material. The stress distribution is calculated by applying a thermal radial profile computed with the CEA/IPSN SCANAIR code. Then, the local stresses are determined in the rim region, in the neighbourhood of a gas bubble.

This 2D-3D FEM approach has been applied successively to REP-Na1 rod, at the time and location of the first failure, and to the postulated RCCA ejection accident in a PWR.

In REP-Na1, the calculated stresses in the cladding, which are due to pure PCMI, are too low to explain the clad failure. In the rim region, only the radial local stress becomes tensile and could explain the rim delamination during the test.

In the RCCA ejection accident, the local stresses calculated at different times in the rim region have been compared to those obtained with a parabolic thermal profile, i.e., during typical normal operating conditions. The "accidental" stress distributions are bounded by the "normal" ones. So, the failure risk due to rim de-cohesion should be very low during such an accident.
I. Introduction

1.1. General context

EdF’s Nuclear Plant Exploitation Division (EPN) is in charge of the production and transport of electric power generated in the French nuclear plants. For economical reasons, it has decided to optimize the nuclear fuel management in order to increase the burn-up discharge limit of its fuel assemblies. This limit, which is today 47 GWd/tM (average burn-up of the most burnt assembly) will be brought, in the next future, up to 52 GWd/tM. This means that the rod average burn-up can reach values up to 55 GWd/tM, and even, higher values can locally be encountered.

This burn-up discharge limit increase will be accepted by the DSIN (the French Nuclear Safety Authority), if EdF can demonstrate that no rod failure, with fuel dispersal in the coolant and mechanical energy generation in the core vessel, may occur during a reactivity initiated accident (RIA), i.e., in a PWR, a rod control cluster assembly (RCCA) ejection.

1.2. The French programme on RIAs

In order to assess this demonstration, CEA/IPSN and EDF/SEPTEN have decided to run together an important R&D programme, which is composed of several parts:

- a series of full-scale RIA tests performed in the CABRI reactor at Cadarache, accompanied by a complete pre-test characterization of the tested rods and post-test non-destructive and destructive examinations [1-2];
- an analytical programme of investigation of the cladding-coolant heat transfer coefficient (PATRICIA tests), which is devoted to experimentally assess the heat transfer correlations in a two-phase flow, during a fast power transient;
- a series of mechanical tests on cladding samples (PROMETRA tests), in order to determine the mechanical properties of highly irradiated zircaloy during fast transients simulated by high strain-rate uniaxial tensile tests and biaxial burst tests [3];
- the conception and qualification of SCANAIR, which is CEA/IPSN’s fuel rod thermomechanical behaviour computer code, adapted to fast transients;
- EDF’s studies, especially on neutronics [4], and fuel rod thermomechanics, like the present analysis.

1.3. The main results of RIA tests in CABRI

Up to now, four RIA tests have been performed in CABRI reactor (the fifth one is scheduled for end July 1995). The main characteristics of these tests are gathered in Table 1 hereafter. All the tests have been run with a sharp power transient (9 ms at mid-height).
Table 1: Main characteristics and results of CABRI RIA tests

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<td>4.5%</td>
<td>63 GWd/tM</td>
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<td>116 cal/g</td>
<td>Rod failure with deposed energy = 15 cal/g; fuel dispersion (6 g)</td>
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<td>6.85%</td>
<td>33 GWd/tM</td>
<td>&lt; 10 μm</td>
<td>203 cal/g</td>
<td>No rod failure; strong PCMI effects: large hoop strains (3.5%) and ridges on cladding</td>
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<td>52 GWd/tM</td>
<td>40-60 μm</td>
<td>117 cal/g</td>
<td>No rod failure; strong PCMI effects: large hoop strain (2.2%) and &quot;waves&quot; on cladding</td>
</tr>
<tr>
<td>REP-Na5</td>
<td>4.5%</td>
<td>64 GWd/tM</td>
<td>20 μm</td>
<td>107 cal/g</td>
<td>No rod failure; NDE are still underway</td>
</tr>
</tbody>
</table>

Destructive examinations on REP-Na1 rod after the test showed that some parts of the peripheral zones of the pellets (the so-called "rim region" [5]), have been removed from the rod. Several cracks have initiated at different altitudes on the cladding (at least, two crack initiation events have been detected by microphones and pressure transducers), and propagated all along the rod. All these observations indicate that several phenomena have been involved together in the rod failure mechanism:

- grain boundary failure in the rim region, which can induce a sudden fission gas release and an overpressurization of the rod [6];
- pellet-cladding mechanical interaction (PCMI);
- cladding material embrittlement, due to waterside corrosion, zirconia peeling and local high hydride concentrations; such highly hydrided zones ("blisters") have been observed on post-test metallographies.

The analysis of the results of the other tests leads to the following conclusions:

- in the failure mechanism described hereabove, the most important parameter seems to be the physico-chemical state of the cladding, and especially the presence of local high hydride concentrations, correlated with local zirconia spalling. This can explain the non-failure of REP-Na5 rod; the only differences between REP-Na1 and REP-Na5 tests are the state and thickness of the zirconia layer;
- the fuel burnup is not the main parameter responsible of the rod failure during a RIA.

1.4. Aim of the present study

The aim of the present thermo-mechanical is twofold:

- First, to give a better understanding in the description of the phenomena involved in a rod during a RIA, in order to physically assess the conclusions of the experimental tests;
- Second, in order to fulfill these objectives, this analysis is focused on the rim behaviour, when submitted to high temperatures and thermal gradients, and on the corresponding deformations in the cladding.
II. High burnup fuel description

At high burnup, the fuel is characterised by the presence of four concentric zones [7]:
- the central zone contains large gas bubbles concentrated mainly at the grain boundaries;
- the second one is a transition area with co-existence of intragranular and intergranular bubbles;
- the third one is characterised by the absence of gaseous swelling and material densification;
- the last external area, called the "rim", shows a large porosity with very small bubbles, and a very small grain size (less than 1 micron).

Many studies have been made on the rim region to get a better knowledge of its structure. So, we have taken into account one of these characteristics, namely its average porosity, which has been estimated to 22% [8]. Since the study is based on continuous medium mechanics, the reduction of the grain size has not been considered.

III. Method of analysis

III.1. General description

In order to determine the influence of the rim region in the failure mechanism, the thermo-mechanical analysis has been done as follows:
- homogenization of the fuel material in the rim region: 3D-meshing of a small cubic fuel material structure, including a spherical gas bubble (representing the rim region with its porosity), and calculation of the mechanical properties (Young’s modulus and Poisson’s ratio) of the equivalent homogenized material;
- 2D-meshing of a pellet and the corresponding piece of cladding, with the properties of the homogenized fuel material applied to the rim region; calculation of global stresses and strains obtained by applying a temperature distribution deduced from SCANAIR calculations;
- Application of the temperature, fission gas pressure and stress tensor components on the small cubic meshing representing the rim material; calculation of local stresses;
- 3D-meshing of a pellet fragment and its facing cladding part, in order to accurately calculate the clad deformations.

The general pattern of this analysis is described on figure 1.

III.2. Basic hypotheses

III.2.1. Meshing

As it is described hereabove, three meshes are needed for this analysis:
- the first one represents one-eighth of a small cube containing a spherical gas bubble, whose respective sizes correspond to the given fuel material porosity. In this first approach, the gas bubbles have been supposed spherical for the sake of simplicity, although they can have lenticular shapes. An exemple of such a mesh is represented on figure 2;
- the second one is a 2D-axisymmetric (r-z) meshing of a pellet and its facing cladding part. The node distribution of the latter mesh has been optimized in order to give correct results (strains and stresses) and minimize the local errors due to edge effects. This mesh is represented on figure 3;
- the third one is the 3D meshing of one-half of a pellet fragment (one fourth of a pellet) with its facing cladding, and is represented on figure 4.

III.2.2. Boundary conditions

On the 3D-mesh, representing a small rim area, the three square faces have been fixed normally to their surfaces and the three truncated faces are submitted to conditions of uniform normal displacement, in order to ensure the compatibility with neighbouring rim material. The minimal boundary conditions should be simply periodic conditions, but these ones are quite representative, considering the size of the structure.

The 2D-mesh, which represent one half of a pellet and the corresponding cladding, is submitted to boundary conditions of symmetry at mid-pellet location. Between pellet and cladding, conditions of unilateral contact are applied. The pellet interface is supposed to be stress-free (the axial compressive strength due to the hold spring is supposed to be completely relaxed during base irradiation). The upper edge of the cladding is submitted to a continuity condition (constant axial displacement through the wall).

The 3D-meshing of the pellet fragment and cladding is submitted, in the vertical plane (X-Z), to the same boundary conditions as for the 2D-axisymmetrical meshing. Along the centerline, conditions of unilateral contact are applied between the fragment edge and a vertical rigid beam, to ensure the contact with the opposite fragment. In the horizontal plane, because one-half of the fragment is modelled, conditions of symmetry are applied on one lateral face of the structure. Unilateral contact is considered between the other lateral face and a rigid wall (contact with the neighbouring fragment) in the pellet. The cladding face is submitted to continuity conditions (normal displacement constant through the wall).

The applied boundary conditions are illustrated on figure 5.

III.2.3. Material properties

In this first approach, the fuel and cladding materials are supposed to be elastic. So, the needed properties are the Young's moduli, Poisson's ratios and thermal expansion coefficients of the fuel oxide and zircaloy. For the fuel material, these properties are formulated as follows [9-10]:

Young's modulus : $E = 229000 \times (1 - 2.3p) \times (1 - 0.00014 \times (T-20))$ (in MPa);

Poisson's ratio : $v = 0.32$ (unitless);

Thermal expansion coefficient (in °C$^{-1}$):

$\alpha = 9.828 \times 10^{-6} - 6.39 \times 10^{-10} \times T + 1.33 \times 10^{-12} \times T^2 - 1.757 \times 10^{-17} \times T^3$ (if $T < 650°C$);

$\alpha = 1.1833 \times 10^{-5} - 5.613 \times 10^{-9} \times T + 3.756 \times 10^{-12} \times T^2 - 6.125 \times 10^{-17} \times T^3$ (if $T > 650°C$)

with :

$p =$ fuel porosity (unitless) (=5%);

$T =$ temperature (°C).

For the zircaloy, we consider the following properties :

$E = 97080 - 58 \times T$ ;

$v = 0.34$ ;

$\alpha = 5.66 \times 10^{-6} + 1.638 \times 10^{-9} \times (T-20)$

with the same notations and units as hereabove.

Other phenomena which could induce some deformations, like creep, swelling, are not considered.
III.2.4. External loading

For all the calculations, the external loading is a temperature distribution which has been obtained with SCANAIR (cf. § 1.1.2.). This temperature field is supposed to be concentric. Figure 6 presents an example of such a thermal loading, which corresponds to the REP-Na1 test, at the instant (74.5 ms after top) and location (80 mm from fuel stack bottom) of the first rupture.

II.2.5. Computer codes used

The meshing pre-processor (GIBI) and the graphic post-processor are parts of CASTEM 2000, CEA's general FEM computer code [11]. The thermomechanical calculations are performed with ASTER, EdF's FEM computer code, which is coupled with GIBI and the graphic post-processor of CASTEM 2000. The temperature distribution, used as input data in thermomechanical calculations, have been computed with SCANAIR.

IV. Results

In order to fulfill the objectives mentioned above (§ 1.4.), this analysis has been applied twice:

- first, to the simulation of REP-Na1 test, at the time and location of the first rod rupture, in order to evaluate the stresses in the rim region of the pellet and in the cladding, and to draw some conclusions concerning the possible failure mechanism;
- second, to the simulation of the postulated RCCA ejection accident at 50 GWd/tM in a 900 MWe PWR, at different times, to determine the stress levels in the rim region and cladding, and to evaluate the failure risk in such an accident.

IV.1. REP-Na1 simulation

The thermal distribution applied to the 2D-mesh has been computed with SCANAIR code at the time (74.5 ms) and location (80 mm from fuel stack bottom) of the first rupture. It is shown on figure 6. The properties of the equivalent homogenized material of the rim region (with a porosity of 22%) have been evaluated. Its Young's modulus is temperature-dependent and is modelled by the following expression:

\[ E = 137781 - 19.43T \]

with \( E \) in MPa and \( T \) = temperature in °C.

Its Poisson's ratio is constant and equal to 0.28.

The stress distributions in the fuel and cladding are respectively shown on figures 7 and 8. As can be seen, the stress tensor in the rim region, at mid-pellet, is compressive in all directions. Its radial component becomes slightly tensile only at the pellet-to-pellet interface. Application of this stress tensor on the 3D-mesh representing a small area of the rim region around a gas bubble (whose pressure and temperature are respectively 44.8 MPa and 550°C deduced from the thermal profile and Van der Waal's equation of state) leads to the stress distribution shown on figure 9. The highest obtained stress is 127 MPa, which is comparable with the material ultimate tensile stress at this temperature (cf. figure 10 [12]). This result could explain the radially delaminated aspect of the rim region, observed on REP-Na1 rod after the test [13].

The strains applied to the cladding have been calculated on the 3D-mesh of the pellet fragment and the corresponding cladding part. It is worthwhile noticing that in this case, the radial strain at mid-pellet location is larger than with the 2D-axisymmetric mesh. Figures 11 and 12 show the pellet deformation obtained with the both meshes. As can be seen, the pellet fragment undergoes axial flexural strains and the pellet takes the shape of a barrel. Such a deformation is not allowed with the 2D mesh, which represents a solid pellet. The radial strain on the cladding at mid-
pellet is 0.29% (instead of 0.23% with the 2D-mesh). This value is supposed to be in the range of the zircaloy fracture limit, when the material is highly hydrided and embrittled.

The main conclusions of this analysis are the following ones:

- When the REP-Na1 rod fails, the rim region is submitted to compressive stresses. Locally, around a gas bubble, the stresses become tensile in the radial direction, but not enough to explain a complete rim de-cohesion and subsequent sudden fission gas release into the free volumes of the rod. The "rim explosion", which was the first hypothesis formulated to explain the rod failure, cannot be invoked as the unique phenomenon responsible for the rod rupture during the test.

- The stresses in the cladding are also too low to explain the rod failure by PCMI. The recently observed hydride "blisters", correlated with zirconia spalling, lead to a severe embrittling of the cladding material and are now supposed to be the most probable sites for crack initiation. A radial deformation of about 0.3% would be sufficient to induce a cladding failure in such conditions. The PROMETRA programme (cf. § 1.2) has been conceived to experimentally assess this point.

IV.2. Postulated RCCA ejection accident in a PWR

The postulated RCCA ejection accident in a PWR is supposed to occur at zero power, with a scram induced by high flux detection at 35% nominal power [4]. This RIA leads to an injected energy of about 65 cal/g in the fuel. In this case, PWR thermal-hydraulic conditions are considered and DNB is activated.

For this case, the temperature distribution has been calculated on a REP-Na3-type rod, submitted to PWR thermal-hydraulic conditions. Two instants are considered: 0.25 s after beginning of transient (when the temperature gradient is maximal in the rim), and 1.513 s (when the temperature is maximal in the rim). The results of these two calculations have been compared to the one obtained with a radially parabolic thermal profile, which corresponds to normal full-power generation in the rod (1000°C at pellet centerline and 450°C at the periphery). The three thermal profiles are shown on figure 13. The main results (global and local stresses in the rim region) are gathered in Table 2 hereafter.

<table>
<thead>
<tr>
<th></th>
<th>RCCA ejection 0.25 s</th>
<th>RCCA ejection 1.513 s</th>
<th>Parabolic thermal profile</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Global stresses</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>in rim (MPa) :</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>( \sigma_r )</td>
<td>-15.9</td>
<td>-18.2</td>
<td>-28.4</td>
</tr>
<tr>
<td>( \sigma_t )</td>
<td>242.9</td>
<td>387.2</td>
<td>587.0</td>
</tr>
<tr>
<td>( \sigma_z )</td>
<td>236.2</td>
<td>367.3</td>
<td>557.6</td>
</tr>
<tr>
<td><strong>Local stresses</strong></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>in rim (MPa) :</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>( \sigma_r )</td>
<td>115</td>
<td>188</td>
<td>301</td>
</tr>
<tr>
<td>( \sigma_t )</td>
<td>640</td>
<td>968</td>
<td>1430</td>
</tr>
<tr>
<td>( \sigma_z )</td>
<td>667</td>
<td>1010</td>
<td>1490</td>
</tr>
</tbody>
</table>

Table 2: Comparison of global and local stresses in the rim region, during a RCCA ejection accident and typical normal operation.

These results show that during a RCCA ejection accident, the global and local stresses in the rim region are lower than during normal operation conditions. This is due to the fact that during a RIA in a PWR, the temperature difference between the
centre and periphery of the pellet is lower than during operation, and the same applies for thermal dilatation. The temperature difference is roughly 550°C during nominal power generation, and it is only 100°C and 200°C, respectively at 0.25 and 1.513 s during the RIA. Consequently, the probability of rim de-cohesion during a RCCA ejection accident must be lower than during normal steady-state and, a fortiori, during transients in normal and off-normal situations.

V. Conclusions

This 2D-3D FEM approach of the thermomechanical behaviour of fuel rods during a RIA has given important results and contributes to better understand the main phenomena which may be involved in the rod failure mechanism.

First, the demonstration has been made that in REP-Na1 test, neither the rim behaviour nor the PCMI can be invoked separately to explain the rod rupture. These results seem to confirm that cladding mechanical behaviour may be the most probable phenomenon responsible for the rod rupture at such a low level of injected energy. This point has been confirmed by the non-rupture of the REP-Na5 rod.

Second, the stress distributions in the rim during a RCCA ejection in a PWR are bounded by the one computed during normal steady-state power generation. Thus, the probability of rod failure by rim de-cohesion and rod over-pressurization may be very low during a RIA.

Some other developments can be made, especially by taking into account the elasto-plastic behaviour of both fuel and cladding materials, in order to try to reproduce the strains observed on the clads after REP-Na2 and REP-Na3 tests. But these further analyses need to better know the mechanical properties of fuel oxide and zircaloy during fast transients, which are not yet fully assessed now.

Acknowledgements

We gratefully acknowledge Mr. Dan PAULSSON, student at University of Göteborg (Sweden) and Institut National des Sciences Appliquées (INSA Lyon, France), who elaborated the method of analysis and performed most of the modelling and computation work concerning its application to REP-Na1 test.
Figure 1

General pattern of the 2D-3D analysis

3D-meshing of a small cubic part of rim region with a gas bubble
Calculation of mechanical properties \((E, \nu)\) of equivalent homogenized material

3D-meshing of pellet fragment and cladding; calculation of cladding strains at mid-pellet

2D axisymmetric meshing of pellet and cladding; calculation of global stress distributions

Application of local stresses and gas pressure on the 3D-mesh; calculation of local stresses around the gas bubble
Figure 2
3D mesh of a small cubic part of the rim region

Figure 3
2D-axisymmetric (r-z) mesh of a pellet and its facing cladding part
Figure 4

3D meshing of pellet fragment and cladding
Figure 5
Boundary conditions applied to the meshes

Top: 3D mesh of a small part of the rim region
Bottom: 2D mesh of pellet and cladding

**uy = 0**  
**stress-free**

**uz = cst**  
**pellet**

**ux = 0**  
**symmetry**

**uy = cst**  
**unilateral contact**

**uz = 0**  
**cladding**
Figure 5 (cont'd)
Boundary conditions applied to the meshes

3D mesh of a pellet fragment and its facing cladding part

- Stress-free: $u_z = \text{cst}$
- Unilateral contact
- Symmetry: $u_z = 0$
- Continuity
- Pellet
- Rigid wall
- Unilateral contact
- Pellet fragment
- Cladding
- Symmetry: $u_y = 0$
Figure 6
Example of temperature profile calculated with SCANAIR

REP-Na1 - Temperature profile at time and location of rod failure
Figure 7
REP-Na1 - stress distributions in fuel pellet

REP-Na1 - Stress distributions at mid-pellet location

REP-Na1 - stress distributions at pellet-to-pellet interface
REP-Na1 - stress distributions in cladding at mid-pellet location

REP-Na1 stress distributions in cladding at mid-pellet location

REP-Na1 - stress distributions in cladding at pellet-to-pellet interface

REP-Na1 - stress distributions in cladding at pellet-to-pellet interface
Figure 9
REP-Na1 - stress distributions in the rim region
(from top to bottom: radial, tangential, axial)
Figure 10
Fuel oxide: fracture tensile stress vs. temperature

STRAIN RATE-0.092 h⁻¹
GRAIN SIZE-~8μm

<table>
<thead>
<tr>
<th>STRESS, kN/cm²</th>
<th>STRAIN, %</th>
</tr>
</thead>
<tbody>
<tr>
<td>200</td>
<td>0</td>
</tr>
<tr>
<td>400</td>
<td>1</td>
</tr>
<tr>
<td>600</td>
<td>2</td>
</tr>
<tr>
<td>800</td>
<td>3</td>
</tr>
<tr>
<td>1000</td>
<td>4</td>
</tr>
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<td>1400</td>
<td>6</td>
</tr>
<tr>
<td>1600</td>
<td>7</td>
</tr>
<tr>
<td>1800</td>
<td>8</td>
</tr>
<tr>
<td>2000</td>
<td>9</td>
</tr>
</tbody>
</table>

TEST TEMPERATURE, °C

ULTIMATE TENSILE STRESS
ELASTIC (PROPORTIONAL) LIMIT
TOTAL PLASTIC STRAIN
MODULUS OF RUPTURE
Figure 11
REP-Na1 - deformations obtained with 2D axisymmetric mesh
(solid pellet)
Figure 12
REP-Na1 - deformations obtained with 3D mesh
(pellet fragment)
Figure 13
RCCA ejection in PWR - thermal profiles applied

RCCA Ejection in PWR - 50 GWd/IM - Thermal profiles

Temperature (°C)

0 1 2 3 4 5

Pellet radius (mm)

- Time = 0.25 s (Grad Trim max)
- Time = 1.513 s (Trim max)
- Parabolic profile
Figure 14
RCCA ejection in PWR
stress distributions in the rim region at time $t = 0.25$ s
(from top to bottom: radial, tangential, axial)
Figure 15
RCCA ejection in PWR
stress distributions in the rim region at time $t = 1.513$ s
(from top to bottom: radial, tangential, axial)
Figure 16
Stress distributions in the rim region with a parabolic thermal profile
(from top to bottom: radial, tangential, axial)
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EVALUATION OF RIA EXPERIMENTS AND THEIR IMPACT ON
HIGH-BURNUP FUEL PERFORMANCE

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Palo Alto, California

1. INTRODUCTION

A recent RIA-type experiment conducted in the CABRI Test Reactor [1] on high burnup fuel resulted in cladding failure at radially averaged stored energy considerably lower than expected. Since the experiment is not representative of LWR conditions, proper understanding and interpretation of the experimental data is needed to evaluate the applicability of the results to in-reactor fuel. Towards this objective, The Electric Power Research Institute (EPRI) has initiated a program to evaluate fuel rod behavior during an RIA, focusing on the response of intermediate and high burnup fuel. A key element of this program is the analysis of the RIA-type experiments using a modified version of the transient fuel rod behavior program FREY,[2], updated to include models for high burnup effects. Analyzing the detailed response of test rods using FREY may help to understand the fuel and cladding mechanisms involved in high burnup RIA events. The analytical approach uses a detailed representation of the fuel rod geometry, coolant conditions and the transient power pulse to evaluate the thermal and mechanical behavior of the test rods.

2. HIGH BURNUP FUEL BEHAVIOR DURING AN RIA

2.1 High Burnup Effects on Fuel Behavior Models

The fuel and cladding undergo microstructural changes during high burnup operation. The important changes in the fuel pellet at high burnup are related to the radial power density and the radial distribution of the fission product inventory. The important changes in the cladding are related to irradiation hardening and outer surface oxidation.

2.1.1 Radial Power Profile

The resonance absorption of epithermal neutrons by the $^{238}\text{U}$ in the pellet periphery results in the buildup of plutonium isotopes in this region. Since the power density is dependent on the fissile content, the increased
production of plutonium near the pellet surface produces a steep increase in
the local power density and the corresponding local burnup. As a consequence,
a pellet at high burnup contains a narrow rim near the periphery which has
accumulated a burnup level that approaches twice the pellet average burnup and
a power density that can be two to three times greater than the pellet
average. Several researchers have developed submodels to estimate the
production of plutonium isotopes and the radial power density profiles as a
function of burnup [3]. The TUBRNP model recently developed by Lassmann and
O'Carroll at the Institute for Transuranium Elements has been shown to give
good results for high burnup fuel [4]. This model has been incorporated into
FREY and used in the analysis of the CABRI experiments.

Because of the relatively small volume of the rim, the steep radial power
distribution at the pellet periphery does not greatly influence the steady
state thermal performance of the fuel pellet. However, in rapid heating
transients, the temperature profile in the pellet is proportional to the power
density, with the peak fuel temperatures occurring in the pellet periphery.
The magnitude of temperature peaking is governed by the radial power
distribution and the power deposition rate. For high burnup fuel, the maximum
pellet periphery temperature can be a factor of 1.5 times greater than the
fuel centerline temperature for very narrow power pulses.

2.1.2 Material Changes Due to Burnup

The properties of the fuel material in the region of high local burnup are
modified by the large inventory of fission product impurities. Recent
investigators have found that the high concentration of gaseous fission
products produces a narrow region of high porosity at the pellet periphery.
This is caused by the coalescence of xenon gas atoms into intragranular
bubbles of submicron size [5]. Also, the fuel undergoes grain subdivision to
grain sizes of the order of 0.5 μm. The occurrence of these pellet changes
has been termed the "rim effect" and the amount of fuel impacted resides in
the outer 5% of the pellet radius.

Correlations for the dependence of the pellet thermal conductivity on burnup
has been obtained for intermediate and high burnup regimes from steady state
experimental programs performed on instrumented fuel rods [6] and thermal
diffusivity measurements on simulated irradiated fuel material [7]. From the experimental data, corrections have been proposed to the fuel thermal conductivity models which account for the decrease in matrix diffusivity as the amount of fission product impurities increase.

The burnup correction terms developed to date apply to the low and medium temperature regimes and use the average pellet burnup instead of the local burnup. The applicability of the correction terms to elevated temperatures (>2000 K) and high local burnups (>80 MWd/kg) has not been demonstrated at this time. This poses modeling difficulties since, during an RIA event for high burnup fuel, the peak temperatures exceed 2000 K and are experienced in the region of the fuel pellet which contains high local burnups. Because of these deficiencies, the pellet modeling approach presented herein considers only the effect of pellet average burnup on the low temperature (<2000 K) regime.

In addition to the effect of matrix fission products, the presence of small bubbles in the pellet periphery increases the local porosity which also impedes heat flow. The effect of the increased porosity can be incorporated as a correction factor on the local thermal conductivity in the same manner as used to correct for the as-manufactured porosity. Recently, Hayes and Peddicord developed a porosity correction factor for the UO₂ thermal conductivity expression which is applicable up to 30% volume porosity [8]. This factor has been incorporated into the fuel thermal conductivity model in FREY.

2.2 Pellet Rim Response to High Temperatures

Under steady state operation, the fuel temperature in the pellet periphery remains below approximately 800 K, and the high porosity rim material is mechanically stable with the fuel material providing adequate containment of the high pressure gas bubbles. As mentioned earlier, during an RIA event for a high burnup rod, the peak fuel temperatures occur initially in the pellet periphery as a consequence of the local power density peaking in the rim region. Some investigators have postulated that, under the effect of the rapidly rising temperature in the rim, the pressurized rim expands rapidly and releases its pressure directly to the cladding causing it to fail [1]. For
this to occur, the gas bubble pressure has to increase beyond the nearly hydrostatic confinement provided by the fuel matrix and the cladding combined. It was further postulated that the extremely small grains produced by the radiation induced grain subdivision could significantly reduce the tensile capacity of the fuel in the rim region. However, experimental characterization of the mechanical instability of the rim region has not been developed to indicate that significant degradation of the fuel tensile strength occurs coincident with fuel restructuring (into small grains) at high burnup.

An analytical model of the thermal and mechanical processes associated with the response of the rim region has been developed using a two-dimensional continuum representation of the rim bubbles, fuel material, and cladding. A finite element model (FEM) was developed in which the gas bubbles were modeled explicitly. A variety of bubble densities were investigated to yield volume porosities between 7% and 30%. The analysis also considered the hydrostatic confinement pressure provided by the cladding. Both the mechanical properties of the solid fuel material within the matrix and the bubble internal pressure (ideal gas law) depended on temperature. The results of the analysis show that the fuel and cladding provide adequate confinement of the increasing bubble pressure up to temperatures of 1800 K. Above this temperature, bubble expansion becomes a function of initial porosity, initial bubble pressure, temperature, and cladding ductility. For example, a rim instability temperature of approximately 1900 K was calculated assuming conservative conditions such as an initial volume porosity of 20%, an initial bubble pressure of 30 MPa, a fuel material confinement equivalent to its low temperature tensile strength, and a cladding confinement equivalent to the very conservative low value of 50 MPa. It is concluded from this study that, under conditions representative of RIA events, the mechanical effect of the rim on the fuel rod response is not significant.

2.3 Cladding Integrity Evaluation Model for High Burnup

In order to evaluate performance differences in the recent test reactor high burnup RIA experiments, a cladding integrity evaluation model based on mechanical properties experimental data as well as integral fuel rod test data
is needed. Such a model has been developed and is utilized to assess fuel performance during the CABRI experiments.

Data from mechanical property tests conducted on high burnup cladding samples [9] indicate that the zircaloy ductility can be affected by the presence of zirconium hydride precipitates (ZrH$_2$). The degree of ductility loss is dependent on the amount of hydrogen in excess of the solubility limit and the orientation and distribution of bulk and local hydrides. Available mechanical properties data for high burnup cladding show large scatter in uniform and total elongation cladding strains when this data is correlated with the average hydrogen concentration in the test sample. The large scatter in the data that is typical is shown in Figure 1. This data was obtained from fuel rods with burnup of about 63 GWD/MTU.

An approach to constructing a cladding performance figure of merit is to utilize the strain energy density (U) which represents the mechanical work performed by the combined states of stress imposed on the cladding. The strain energy density is defined by the following relationship:

$$U = \int \sigma_{ij} \, d\varepsilon_{ij}$$

(1)

where $\sigma_{ij}$ are the coordinate stresses and $d\varepsilon_{ij}$ are the coordinate strain increments, taken over the time history and excluding the compressive stresses.

The strain energy density at the material limits were determined for the available test data from irradiated cladding [9]. In performing this analysis, the total elongation strains were used to define the material strain capability. Further, in order to complete the stress-strain curve, the elastic strain was added to the measured plastic elongation data. The elastic strain was obtained for each test value by simply dividing the yield strength obtained in the test by the elastic modulus calculated from MATPRO [10].

The strain energy density determined from the total elongation data represents the critical strain energy density ($U_c$). The strain energy density versus the
total elongation strain is shown in Figure 2 for the test data (symbols) and the analytical model. The latter is obtained by integrating the MATPRO constitutive equation up to the total elongation strain measured in the test. As shown in the figure, good agreement between the test data and the MATPRO-based analytical model is obtained. The results presented in Figure 2 were derived from the axial tension data shown in Figure 1. Similar agreement was obtained for the other test configurations, namely ring tension and burst tests.

The values presented in Figure 2 for the \( U_f \) determined from the test data indicate that, for a given temperature, the strain energy density is linearly dependent on the total elongation and, for a given total elongation, the strain energy density decreases with increasing temperature. However, the \( U_f \) can vary considerably for a given temperature, indicating that other factors contribute to the material limit. \( U_f \) is a function of many state variables, including temperature, fast neutron fluence, material anisotropy, strain rate and hydrogen content. The test data shown in Figure 2 was used to develop a relationship for the critical strain energy density as a function of hydrogen content, temperature and loading direction. For highly irradiated cladding, the effect of fast fluence is assumed to have already saturated, and only the effects of temperature, material anisotropy and hydrogen content need to be considered in developing a relationship. In addition, the test data did not cover a wide enough range of temperatures or strain rates to permit development of an explicit dependence for \( U_f \). However, the test temperatures were representative of nominal operating conditions.

The materials test data was separated into two groups: (1) axial tension tests, and (2) ring tension and tube burst tests. These groups represent the two major loading directions (axial and hoop), and a strain energy density relationship was determined for each group. The total strain energy density was determined by a linear combination of the two separate groups as a means to represent a multi-axial stress state. This approach is an approximation to modeling the anisotropic behavior exhibited by the test data. For a more complete analysis, it would be more appropriate to introduce anisotropy factors in the combination of the two data groups. The combined relationship for \( U_f \) as a function of hydrogen is used to evaluate the capacity of the
cladding to accommodate the deformations during the RIA experiments. The developed relationship between the critical strain energy density \( U_f \) and average hydrogen content is shown in Figure 3.

The data utilized in the present development exhibit other features in addition to the large scatter shown in Figure 3. These are: (a) the limited data set displays a temperature dependence; however, sufficient data was unavailable to incorporate this dependence; (b) although not shown in the figure, the uniform elongation shows a weaker dependence on hydrogen concentration than is displayed by the total elongation; and (c) strong anisotropy is present, with the axial critical strain energy density displaying a sharper decrease with hydrogen concentration than the hoop critical strain energy density. This is somewhat surprising since axial plasticity is generally greater than hoop plasticity. The model is applied to the analysis of the CABRI tests, as will be discussed in Section 3.

3. ANALYSIS APPROACH

A major objective of the EPRI program is to interpret the fuel rod behavior exhibited by the test rods irradiated to high burnup and tested in RIA-type experiments. This requires two analysis capabilities: (1) steady state, to establish the initial conditions; and (2) transient, to compute the transient response. The analysis tools used in the RIA assessment were ESCORE and FREY [11,2]. Both programs belong to the family of LWR fuel rod analysis capabilities provided by the Electric Power Research Institute. ESCORE is a best estimate analysis program which describes the thermal and mechanical performance of LWR fuel rods under steady-state or quasi steady-state operating conditions. FREY is a finite element-based best-estimate analysis program designed to compute the transient thermal and mechanical behavior of an LWR fuel rod during both normal and off-normal events. An extensive verification and validation of FREY for transient analysis has been performed using fuel rods irradiated in experimental programs conducted in the Power Burst Facility (PBF) and the Transient Reactor Test Facility (TREAT) [12]. Modifications were incorporated into FREY to represent the high burnup effect discussed earlier. These modifications included the TUBRNF submodel for the

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radial power profile and a fuel thermal conductivity model which included the effects of local porosity and burnup.

The FREY geometric model consisted of a detailed representation of the fuel pellet in the radial direction to properly account for the local temperature peaking in the pellet rim region. In the radial direction, an expanding mesh was used with fine detail in the 500 to 1000 μm region near the pellet periphery and a coarse mesh in the remainder of the pellet. A sensitivity analysis has shown that seven 8-node quadrilateral elements were sufficient to obtain an accurate representation of the radial temperature profile. A total of 80 equal-sized radial divisions were used in the TUBRNP radial power profile model to capture the local burnup and power density profile at high burnup.

The heat transfer conditions at the cladding outer surface are normally computed in FREY using a one-dimensional flow channel model for flowing water or water/steam mixtures. Heat transfer coefficient correlations representing the entire boiling curve are included in the program. However, for the experiments conducted in the CABRI facility, the coolant conditions were not characteristic of typical LWR conditions. The coolant was liquid sodium. It was required, therefore, to input the appropriate surface heat transfer coefficients and coolant temperatures as functions of position and time in the analysis of these experiments.

3.1 CABRI Test Series

The analysis results that will be presented with some detail in this paper are for the first three transient tests performed in the CABRI test facility within the REP Na test series [1]. Only very limited results are presented for REP Na-4 and REP Na-5. The test rods selected for the CABRI reactivity experiments had been base irradiated under representative LWR conditions to burnups ranging between 33 GWD/tU to 63 GWD/tU.

The power pulses used in the CABRI facility are extremely sharp and narrow with an approximate 10-millisecond (ms) width at half maximum power (FWHM), due to the reactivity insertion control system. However, the pulse shapes generated in the CABRI REP test series are similar to those used in the NSRR
and PBF experiments [13,14]. A test capsule device was used which contained instrumentation to monitor the coolant flow and pressure at the entrance and exit of the flow channel, coolant temperature throughout the test capsule, fuel column and cladding elongation, and microphones to detect cladding failure. Prior to and after each test, the test rods underwent extensive nondestructive examinations including profilometry, eddy current testing, neutron radiography and gamma scanning. Destructive examinations have been performed or are planned.

The first CABRI experiment, REP Na-1, was performed on a refabricated segment from a 17x17 PWR fuel rod irradiated for five cycles in the Gravelines reactor [1]. The segment was 568 mm long and was extracted from the full-length fuel rod at the axial location corresponding to the fifth and sixth spacer grids. After extraction, end caps and an upper plenum were added using the FABRICE procedure. The average burnup of the test segment was 63 GWd/tU. The REP Na-1 test consisted of an energy pulse which deposited a total of 116 cal/gm (UO₂). In-pile instrumentation detected fuel rod failure during the power pulse at an estimated radially averaged fuel enthalpy increase of approximately 15 cal/gm (UO₂).

The second CABRI experiment was performed using a full-length rod base irradiated in the BR3 reactor to an average burnup of 33 GWd/tU [1]. The test rod contained a fuel column 1000 mm in length and was enriched to 6.85% ²³⁵U. The only pre-test modification introduced was the replacement of the original fill gas with helium at atmospheric pressure. The REP Na-2 test consisted of an energy pulse of 202 cal/gm (UO₂).

The third CABRI experiment, REP Na-3, was performed using a 436-mm fuel rodlet from a segmented rod base irradiated in the Gravelines reactor for four cycles [1]. The 4.5% ²³⁵U enriched test rodlet achieved an average burnup of 53 GWd/tU. The rodlet was located at the grid span between spacers 5 and 6. The test rod was modified only to exchange the fill gas with helium at 3 atmospheres. The REP Na-3 test consisted of an energy pulse of 120 cal/gm (UO₂). A representative fuel rod power pulse used in the analysis of the CABRI rods is shown in Figure 4. The peak rod power for all tests exceeded
20,000 kW/m, and the power deposition was completed in less than 50 milliseconds (ms).

3.2 Analysis Results

The rapid power pulses used in CABRI resulted in almost complete adiabatic heatup of the fuel pellet. This results in fuel temperature profiles which are peaked in the pellet periphery and decrease in temperature towards the pellet center. This behavior is shown in Figure 5 at three different energy levels for REP Na-1 during the power deposition. A more typical parabolic temperature distribution occurs long after the completion of the power pulse ($t > 1$ second) when heat conduction mechanisms become more dominant.

A summary of the results are shown in Table 1 for the three CABRI cases as predicted by FREY. Included in Table 1 are the maximum temperatures at the fuel pellet centerline and pellet periphery and at the cladding inner surface. These results correspond to the axial location of the peak power within the test rod. The results of the temperature predictions highlight the amount of temperature peaking that occurs in the pellet periphery as a consequence of the Pu buildup on the radial power distribution. For the test rods with high burnup, the pellet periphery reaches a peak temperature which is approximately 500 K higher than the centerline. This type of temperature profile which is strongly peaked towards the pellet periphery results in a deformation shape of the pellet that has a bulge in the axial center of the pellet compared to the pellet ends. This deformed shape resembles a barrel and is the opposite of the more characteristic hourglass shape observed under steady state conditions. The lower burnup test, REP Na-2, experiences less temperature peaking at the periphery and as a consequence produces pellet ridges indicative of the hourglass shape.

The radial and axial displacements during the experiment (max) and after the test (residual) are shown in Table 2 for both the fuel and cladding. In addition, the results obtained from REP Na-2 and Na-3 determined by either in-pile instrumentation or PIE measurements are included for comparison, where appropriate. For the radial displacements, the mid-pellet displacements were used from the PIE profilometry data. The amount of residual radial
displacement in the cladding shows a strong dependence on the amount of deposited energy. For REP Na-2, the residual displacements are almost twice that of the other two tests, consistent with the higher deposited energy.

The cladding radial displacement profile computed for REP Na-2 is compared to the PIE profilometry data in Figure 6. Good agreement is observed in the magnitude of the cladding radial displacement. It is noted that the shape of the profile shows a shift toward the lower portion of the rod for the computed values. This shift is attributed to the uncertainty in the axial power profile used in the analysis. The power shape used in the analysis was determined during the pre-test power calibration at low power steady state operation. This power shape displayed a clear shift towards the bottom end of the tested portion of the rod. However, the transient axial power shape could have moved towards the top of the rod as a consequence of the reactivity insertion control system.

Cladding performance predictions were attempted using the critical strain energy density formulation \( U_f \) presented in Section 2.3. Comparisons were made to the computed strain energy density determined from the transient analysis. The computed strain energy density is given by the following relationship:

\[
U(t) = \int_0^t \sum \sigma_i(t) \Delta \varepsilon_i(t) \, dt \quad i = 1, 2, 3
\]

where \( \sigma_i \) and \( \Delta \varepsilon_i \) are the tensile hoop and axial stress and incremental strain components, respectively. For a fuel rod cladding, the three stress components correspond to the radial, hoop and axial directions. In the calculation for \( U(t) \), the compressive radial stress was not included because it does not contribute to the formation of material separation.

Figure 7 shows the time history of the strain energy density during the initial energy deposition of the REP Na-1 transient. The failure time indicated by in-pile instrumentation is shown as 74.5 msec [1]. At this time, the computed strain energy density is approximately 0.35 MJ/m³, which corresponds to a change in radially averaged fuel enthalpy of 15 cal/gm (UO₂).
The failure strain energy density is determined by the cladding integrity evaluation model to be approximately 10 MJ/m$^3$, assuming that the test rod average hydrogen content was of the order of 750 ppm. The computed failure time is nearly 80 msec and corresponds to an increase in the radially averaged fuel enthalpy of approximately 50 cal/gm (UO$_2$).

Several factors could contribute to the difference between the predictions and the measurements for REP Na-1. These include power history uncertainties, in-pile instrumentation uncertainties, or uncertainty in the initial state of the test rod. Regarding the first two factors, the rapid power insertion during the initial portion of the test produces fuel rod changes that occur over time periods of 1 or 2 msec due to the almost exponential increase in power level. As a consequence, uncertainties in the start of the power transient or in the in-pile data collection devices can result in a shift in the power level (energy level) at failure. It is beyond the scope of this analysis, however, to determine if such uncertainties existed in the test.

Uncertainties in the initial state of the test rod may have significantly contributed to the early failure time observed in REP Na-1. Pre-test eddy current measurements indicate the possibility of both external and internal incipient cladding defects. These defects could be localized hydriding at the external surface, as previously observed in some high burnup cladding which has experienced an unusual amount of surface oxide spallation [15], or internal part-wall defects consistent with PCI. Occurrence of either or both types of defects could produce the discrepancies observed between the calculated and measured failure times for REP Na-1.

The failure energy density approach was applied to the analysis for tests REP Na-2 and REP Na-3. Using the pre-test surface oxide measurements, the cladding hydrogen content was estimated to be 30 ppm for REP Na-2 and 230 ppm for REP Na-3. Based on these values, the critical strain energy density as determined using the relationship in Section 2.3 is 90 MJ/m$^3$ and 55 MJ/m$^3$ for REP Na-2 and REP Na-3, respectively. The maximum values attained during the transient are computed to be significantly lower than the computed cladding limit, indicative of the large amount of ductility remaining in the cladding.
for these rods. The margin to the critical strain energy density for REP Na-2 and REP Na-3 appears to be a factor of 2 in both cases.

Preliminary analysis results have also been obtained for the recent CABRI tests REP Na-4 and REP Na-5. The analysis for REP Na-4 and REP Na-5 used the initial conditions of REP Na-1 for rod geometry and fuel rod burnup. For REP Na-5, the outer surface oxide thickness was decreased to 20 μm to correspond with a test rod that was refabricated for the lower portion of a high burnup rod. For REP Na-4, the pulse width was increased and the energy deposition was decreased. The results of these preliminary analyses will be discussed in the following section.

4. DISCUSSION OF RESULTS

Although the analysis approach presented is preliminary, the results of applying the EPRI fuel rod methods to the CABRI RIA experiments provided valuable insight into the behavior of the test rods. A detailed evaluation of the fuel pellet thermal performance has been obtained, including the temperature peaking in the pellet periphery during the nearly adiabatic heat-up portion of the transient. The amount of temperature peaking is dependent both on the burnup and the pulse shape. The burnup dependence is produced by the radial power profile within the pellet, and the pulse shape influences the amount of heat conduction to the cladding. The narrow pulses used in test facilities accentuate the temperature peaking that occurs during the energy deposition.

Comparison of the predicted cladding strains with estimated values from preliminary post-test examinations are shown in Figure 8 for all three tests. It should be stated, however, that the REP Na-1 prediction is shown as a line, since displacement measurements were precluded by the failure. The close agreement, shown in Figure 8, between the measured and predicted values gives support to the present methodology. Further comparisons to the in-pile and PTE data for both fuel column, Table 2, and cladding deformation profile, Figure 6, indicate an overall agreement with the analysis results. Some differences are observed which can be attributed to the uncertainties associated with the axial power distribution during the transient. From these
analysis results, it can be concluded that the behavior of the fuel pellet during an RIA event is not significantly affected by high burnup. This is contrary to initial opinions that high burnup fuel could behave in a detrimental manner during an RIA event. Some differences are observed in the cladding radial deformations observed for REP Na-3 which could be attributed to such high burnup effects as fuel thermal conductivity degradation by solid or gaseous fission products. These factors will be investigated further.

An attempt was made to utilize a new cladding integrity evaluation model in the analysis of the CABRI tests. The maximum strain energy density calculated for each CABRI test is shown in Figure 9 along with the model relationship. It is observed that tests REP Na-2 and REP Na-3 reside well below the curve. In addition, the value for REP Na-1 determined at the time of failure is also below the curve. As already mentioned, several factors may have contributed to this behavior, the most significant of which is the presence of pre-existing cladding damage. Finally, preliminary values for REP Na-4 and REP Na-5 determined from initial calculations similar to REP Na-1 (REP Na-5 was assumed to contain a lower outer surface oxide layer) are shown in Figure 9. The cladding integrity evaluation model predicts that REP Na-5 should survive the power pulse and that REP Na-4 would reside on the boundary with the possibility of failure. These results indicate that the cladding evaluation model is conservative when correlated with hydrogen.

The ability of a test rod to survive severe test reactor power pulses seems to depend on the amount of residual ductility remaining in the cladding. It is the cladding ductility that accommodates the fuel thermal expansion produced during such severe energy depositions. Since cladding ductility is influenced by temperature, this behavior is particularly important under conditions where cladding heat-up is limited due to excessive external cooling (sodium coolant) or nonexistent heat conduction (narrow pulse). For these situations the effects of pre-test operational conditions, such as fast neutron fluence, corrosion hydrogen content and distribution non-uniformities, play an important role in the behavior of the cladding in the test.
5. CONCLUSIONS

The use of the EPRI fuel rod analysis methodology has allowed for interpretation and assessment of the behavior exhibited by the CABRI test rods. The analysis results show reasonable agreement with the observed thermal and mechanical response of the REP Na-2 and Na-3 test rods. It can be concluded from the analysis results that the fuel pellet does not exhibit unexpected behavior as a consequence of high burnup. The survivability of a fuel rod under RIA conditions is influenced by the amount of cladding ductility available to accommodate the fuel thermal expansion during the energy deposition. Based on the analysis, the capability of REP Na-1 to withstand the energy deposition during an RIA may have been seriously compromised by the presence of pre-existing damage.

6. REFERENCES


### Table 1

**TEST ROD TEMPERATURES**

<table>
<thead>
<tr>
<th>Maximum Temperature (K)</th>
<th>REP Na-1</th>
<th>REP Na-2</th>
<th>REP Na-3</th>
</tr>
</thead>
<tbody>
<tr>
<td>Pellet Periphery</td>
<td>2660</td>
<td>2875</td>
<td>2480</td>
</tr>
<tr>
<td>Centerline</td>
<td>1940</td>
<td>2780</td>
<td>1960</td>
</tr>
<tr>
<td>Clad Inner Surface</td>
<td>935</td>
<td>1025</td>
<td>935</td>
</tr>
</tbody>
</table>

### Table 2

**FUEL ROD DISPLACEMENTS**

<table>
<thead>
<tr>
<th>Test</th>
<th>Radial (μm) Residual / Max</th>
<th>Clad Axial (mm) Residual / Max</th>
<th>Fuel Axial (mm) Residual / Max</th>
</tr>
</thead>
<tbody>
<tr>
<td>REP Na-1</td>
<td>Predicted: 66 -</td>
<td>Predicted: 5.0 6.75</td>
<td>Predicted: 1.3 7.0</td>
</tr>
<tr>
<td></td>
<td>Measured: -</td>
<td>Measured: -</td>
<td>Measured: -</td>
</tr>
<tr>
<td>REP Na-2</td>
<td>Predicted: 120 -</td>
<td>Predicted: 5.8 9.2</td>
<td>Predicted: 9.0 12.5</td>
</tr>
<tr>
<td></td>
<td>Measured: 112 -</td>
<td>Measured: -</td>
<td>Measured: 4.0 10.0</td>
</tr>
<tr>
<td>REP Na-3</td>
<td>Predicted: 65 -</td>
<td>Predicted: 3.8 5.1</td>
<td>Predicted: 2.8 5.4</td>
</tr>
<tr>
<td></td>
<td>Measured: 84 -</td>
<td>Measured: -</td>
<td>Measured: -</td>
</tr>
</tbody>
</table>
Figure 1. Total Elongation Data Versus Average Hydrogen Concentration

Figure 2. Strain Energy Density at Failure Versus Total Elongation for Axial Tension Tests at 313 K, 573 K and 673 K
Figure 3. Cladding Evaluation Model - Comparison to Mechanical Properties Data

Figure 4. Test Rod Average Power History
Figure 5. Fuel Radial Temperature Profile

Figure 6. Cladding Outer Surface Radial Displacements
Figure 7. Strain Energy Density as a Function of Time

Figure 8. Predicted versus Measured Residual Cladding Strains for CABRI Test Series REP Na-1, REP Na-2, and REP Na-3
Figure 9. Comparison of Computed Strain Energy Density With Cladding Integrity Evaluation Model for CABRI Tests
INTRODUCTION

With the world-wide trend to increase the fuel burnup at discharge of the LWRs, the reliability of high burnup fuel must be proven, including its behaviour under energetic transient conditions, and in particular during RIA. Specific aspects of irradiated fuel result from the increasing retention of gaseous and volatile fission products with burnup. Under overpower conditions, they can lead to solid fuel pressurization and swelling, causing severe PCMI, or after release, to internal pressure increase with further consequences including clad ballooning. These processes may result in fuel rod failure.

To establish the impact of the burnup increase on the failure threshold and final consequences in term of mechanical energy, is the main purpose of the safety evaluations.

In the last years, substantial effort has been made to enlarge the existing database issued from SPERT and PBF experiments (Ref. 1). Indeed, the number of tests with irradiated fuel was limited to 13 in total, and restricted to relatively low burnups (≤ 32 GWD/tn). In the Nuclear Safety Research Reactor (NSRR) of JAERI, experimental research on irradiated fuel rod behaviour has been conducted since 1989 (Ref. 2), using short fuel rods refabricated from commercial water reactors or highly enriched short test fuel rods pre irradiated in the Japan Materials Testing Reactor (JMMTR).

In the CABRI test reactor of the CEA (FRANCE) the CABRI REP-Na experiments, first phase of the CABRI-REP programme (Ref. 3), has been initiated in 1993, using commercial PWR fuel (short or refabricated). This programme is devoted to high burnup fuel studies (> 30 GWD/tU) under the RIA conditions, and the first part is focused on the possible occurrence of an early clad failure.

Despite some unrepresentative aspects of all these tests (Table 1), experimental results have shown that fuel irradiation leads to a significant reduction in the failure enthalpy threshold comparatively to fresh fuel. At very high burnup (50 ~ 60 GWd/t), a drastic reduction is observed, probably due to fission gas accumulation, rim formation and clad embrittlement due to hydrogen accumulation.
Fast and energetic transients, with conditions similar to RIAs, have, in the past, been performed and analysed with LMFBR fuel, in the frame of the safety studies (Ref. 4). Some basic characteristic phenomena have been observed, which are also clearly observed in LWR fuel under RIA conditions. So, a qualitative assessment of the phenomenological behaviour of irradiated fuel in RIA can be made, allowing to estimate clad strain and the risk of clad failure. In the underlying mechanisms, fuel and clad behaviour is involved and strongly correlated. So, for a reliable modelling of the transient fuel rod behaviour under RIA conditions, which is the purpose of the French code: SCAN AIR (Ref. 5), their respective influences must be identified, the loading from the irradiated fuel and the response of the irradiated clad. In the French experimental programme, analytical tests have been defined to study the clad mechanical behaviour for different types of strain, taking into account the influence of temperature, strain rate, burnup and the level of corrosion and hydriding (Ref. 6). However, the source of mechanical loading under RIA conditions must also be identified. Even if characteristic features like fuel cracking and/or swelling, induced by transient fission gas behaviour, are relatively well understood and currently modelled in the LMFBR field (Ref. 7, 8), additional knowledge is necessary in the field of LWR fuel, taking into account the specific aspects of a commercial PWR fuel, correlated to the pin design and influenced by the irradiation conditions.

The potential source for solid fuel pressurization is largely enhanced by the low gas release during irradiation ($\leq 5\%$) and to the state of fission gases in a confined form (solution or micro bubbles) due to low temperatures in operating conditions.

In the high burnup fuel, high fission product accumulation induces micro structural modifications, already during nominal irradiation, particularly in the "rim" region, leading to an extremely fine-grained structure with an increasing porosity containing the major part of created fission gases. This leads to specific behaviour under RIA conditions correlated, not only to this high fission gas content, but also to the change in the mechanical properties of the highly irradiated fuel. Energetic considerations show that such a situation increases strongly the risk of an early clad failure at a low enthalpy level, during the heating phase. The experimental results of CABRI-REP and NSRR, might find their final understanding to a great extent in the fission gas induced transient swelling.

The noticeable features observed in the tests are: significant increase of fission gas release in high burnup fuel and important fuel swelling at low enthalpy levels. These observations seem at first view, inconsistent with common understanding or incoherent between them. Explanations are postulated which underline the importance of the initial state. But, it seems obvious that additional theoretical and experimental investigations are necessary, not only in the transient fuel rod behaviour, but also in the knowledge of the fuel and clad evolution under irradiation conditions.

1. PHENOMENOLOGICAL BEHAVIOUR OF A PWR FUEL IN RIA

Some relatively well defined steps can be considered in the transient development. For each of them, assessment of clad deformation and/or risk of failure can be made, taking into account the burnup level, the enthalpy level, and the mechanical and thermal clad behaviour.
1. Heating phase

The heating rate of more than \(0.05\) K/s, induces a fine fragmentation of the fuel at the granular scale, due to the over-pressurization of the grain-boundary bubbles. This mechanism induces stresses between the grains which exceed the fracture stress of the fuel, leading to grain boundary rupture (Fig. 1). Clear evidence of this "brittle" fuel fragmentation has been obtained, with the I MR fuel, from the SILNE experiments (Ref. 9), at a temperature threshold of 2300 - 2400°K. The threshold is clearly lower in PWR fuel (\(\leq 1500\) K) due to lower irradiation temperatures.

This grain-boundary separation occurs first in the outer zones where the transient thermal level is highest and the relative increase between irradiation and transient is the most important (Fig. 2). However, the width increases with energy deposition, and leads to a fast liberation of boundary gas. Nevertheless, from experimental observations and theoretical considerations (Ref. 4), it has been concluded that this mechanism can induce some PCMI only at a sufficiently high enthalpy level as far as medium burnup is concerned (\(< 40\) GWd/tU). So, the risk of clad failure during the heating phase is unlikely with standard LWR fuel in the absence of RIM formation. But at higher burnup, the extremely fine-grained micro structure developed in the rim region leads to an important increase of the grain boundary gas (nanometer inter granular bubbles and large pores). In paragraph II, with the assumption of a pores/bubbles repartition based on experimental observations, we show that sufficient energy could be liberated from this gas (after grain boundary separation) to induce severe fast PCMI at a low enthalpy level. So, with the loss of clad ductility induced by oxidation and hydride formation in high burnup fuel, this could explain the early clad failure observed in REP-NaI (63 GWd/tU) at very low fuel enthalpy (\(\sim 30\) cal/g). Furthermore, in the case of this test, the brittle behaviour of the clad could be enhanced by the presence locally of characteristic hydride accumulations ("sun bursts") in the irradiated pin before the transient (Ref. 10, 11).

2. Cooling phase

This concerns the phase after the peak fuel enthalpy, with relatively quick cooling in the outer zones, but still slow temperature increase then slow decrease in the inner zones (Fig. 3). Additional phenomena inside the fuel are involved in the mechanisms of clad loading induced by fission gases, for which however longer time is necessary:

- intragranular migration,
- fission gas induced swelling,
- fission gas release.

All these phenomena are strongly correlated to the fuel temperatures and thermal gradients, and increase with increasing fuel enthalpy. In particular, the general understanding of fission gas behaviour shows that the two first processes can be activated in such energetic transients, only at significant temperatures (2000 to 2500°C, depending on transient duration). But, concerning the fission gas release, experimental results show that even below the enthalpy levels where the thermally activated migration is generally observed,
substantial gas release takes place in high burnup fuel: up to 22% in NSRR HBO2 test (~ 50 GWd/tU) at only 40 cal/g (± 15%). It seems probable that this gas is issued in the major part from porosity and grain-boundary bubbles, so is strongly correlated to the end-of-life (EOL) state. The release takes place during transient due to thermomechanical stresses which induce cracks in the fuel.

Consequently, different phases of clad loading can be identified all along the cooling phase, their respective influences and the risk of clad failure is depending on initial conditions, fuel burnup, energy deposition and transient rate.

- **PCMI at the beginning of the cooling phase (some tens msec)**

  Most of the clad failures observed in the fast RIA tests (JMTR rods, SPERT-CDC, PBF) occur during this phase from an enthalpy level around 150 cal/g. This failure, at an early stage of the transient, is most probably correlated to PCMI, induced by intragranular migration in the fragmented zone, and to its effect on a relatively cold cladding. This cold state and the high strain rate favour a brittle behaviour, and clad failure can occur if the rupture stress is exceeded, with no or limited plastic deformation, depending on clad embrittlement and initial conditions (cold or hot start-up). It might come out that, in the JM series, the low fast fluence and the cold initial conditions enhance the risk of failure.

- **PCMI during the later cooling phase**

  This load mechanism results from fuel heating and swelling in the inner zones, induced by the growth of overpressurized bubbles and pores, when the temperature level is sufficient. Significant clad plastic deformation can occur with clad ductility increase (with T7), and increases with fuel enthalpy. But the risk of failure will depend on the clad strength which is degraded at higher temperatures, this underlines the importance of the clad temperature evolution and heat transfers (fuel-clad, clad-coolant), as also on the initial conditions and transient duration.

  Unfortunately, from this stage in transient, the unrepresentative aspect of RIA tests conditions (Table 1) become more important, especially in which concerns the clad temperature evolution, and so its mechanical behaviour. But, as a consequence of the unrepresentative aspect of coolant conditions, the fuel thermal evolution is not representative of the reactor case (Fig. 3). Furthermore, if clad strain depends on dynamics of fuel swelling, inversely, fuel swelling depends on clad mechanical behaviour, these two aspects: fuel loading and clad response are strongly correlated.

  Mechanisms of fission gas induced swelling are relatively well known and currently modelled: growth of overpressurized fission gas bubbles (effect of heating and coalescence) by vacancy diffusion and plastic creep. But, these mechanisms are interdependent with the hydrostatic stress field inside the fuel, and so with the clad mechanical strength. Besides, on the one hand, the plastic behaviour and the creep rate of irradiated fuel is not well known, and on the other hand, mechanical properties of the
clad depend on its evolution under irradiation conditions and its thermal history during the transient.

Consequently, in the present state of knowledge, a clad failure by PCMI during this later phase is difficult to appreciate. Nevertheless, such eventuality must be carefully examined, mainly in case of slower RIA transients (pulse width ~ 80 ms).

- **Cladding rupture due to high internal pressure**

This type of failure occurs when the rod's internal pressure exceeds external pressure, together with a high clad temperature \(T \geq 800^\circ\text{C}\) which decreases the rupture stress. So, such possibility must be considered in the later phase of cooling, when the PCMI phase induced by fuel swelling saturates, and fission gas release is enhanced by fuel cracking and gap reopening. The resulting rod internal pressure increase can cause the clad ballooning and subsequent burst failure. The risk of such a later failure by this mechanism must be assessed in case of no rupture by PCMI. This risk is probably higher in high burnup fuel with increasing gas release in solid state as observed in NSRR and CABRI experiments (Table 2), which is very likely due to the important part of porosity and grain boundary gas in the EOL state. Nevertheless, the major point is not the final gas release measured after the test, but the kinetics of this gas release, because the risk of burst failure will be deduced from the combination of rod internal pressure evolution and thermal clad history. These kinetics are not presently available.

II. SPECIFIC ASPECTS OF PWR FUEL

Specific aspects of PWR fuel result from pin design and irradiation conditions:

- high plenum pressure,
- low linear power,
- thermal neutronic flux.

Consequently, this induces the main characteristics of irradiated fuel and the further consequences in RIA transients.

1. **Low irradiation temperatures**

Typical center and surface temperature evolutions are given in figure 4a, and a radial temperature profile at beginning of life (BOL) and EOL in figure 4b for a 63 GWd/tU burnup. The level is largely below the melting point, and also below the temperature level of a LMFBR fuel \(\sim 1000^\circ\text{C}\) less in the central part. So, in contrast to this last one, no modification of micro structure bound to the thermal level takes place in operating conditions in a PWR fuel: grain growth, evaporation condensation phenomena, pore migration, diffusion processes, gaseous swelling ..., all these phenomena being activated from temperatures \(\geq 1200^\circ\text{C}\). That means also, that a small overpower can activate some of these mechanisms if the thermal level and/or duration are sufficient. For example, under
RIA conditions, even with a low energy deposition, the thermal level is rapidly higher than under irradiation, mainly in the outer part due to quasi adiabatic energy deposition from an isothermal state during the heating phase (fig. 2).

2. High fission gas retention

This characteristic is directly correlated to the low temperatures. In PWR fuel, gas release proceeds mainly by athermal processes (recoil, ejection and saturation). Only in the central part of the fuel, some thermal release takes place in high burnup fuel. Anyway, limited plenum pressure (< coolant pressure), and so limited gas release (~ 5 %) is imposed in operating conditions by the design criteria of PWR fuel.

Consequently, the fission gas retention increases quasi-linearly with the fuel burnup (figure 5). At an average burnup of 60 GWd/tU, the mean fission gas retention is about 1.6 cm$^3$/g. This corresponds to a physical volume of 0.105 cm$^3$/g under conditions : 2500°K, 14 MPa, so of the same order as the fuel volume. Clearly, under irradiation, due to the low temperatures, most of the gas is in solution or in micro bubbles for which the surface energy enables to sustain high gas internal pressure. That explains why no significant gaseous swelling is observed in irradiated fuel. But that means also that most of the potential energy from fission gas is available for solid fuel pressurization and/or plenum pressure increase. Effective contribution of fission gas will depend on the possibilities to liberate this stored energy. In energetic transients, all the physical processes, characteristic of the fission gas behaviour (migration, coalescence, swelling ...) which are thermally activated, can increase drastically the effective contribution of fission gas to the global behaviour. These last effects increase with the thermal level, but depend also on the stress field inside the fuel. And even, if the external clad restraint is the final limiting factor, the fission gas induced swelling is limited first of all by the fuel strength.

A significant difference between the temperature level under irradiation, and the temperature threshold from which all the phenomena correlated to the diffusion processes are activated must be postulated. This temperature threshold depends on the heating rate. In fast heating rates ($\geq 10^5$ K/s), no sufficient time is available to activate significantly the diffusion phenomena, even at high temperatures. But a desequilibrium is established in the thermal levels : transient temperatures compared to the irradiation level, can induce mechanical stresses issued from overpressurized bubbles inside the fuel, even with a relatively low energy deposition (fig. 2). If the fracture stress is exceeded, brittle fracture will occur in the fuel. We have seen in 1.1. that brittle fracture along grain boundaries is the mechanism postulated to explain the grain boundary separation observed in the outer zones of the fuel after RIA (fig. 1). For the moment, no transgranular brittle fracture is postulated in RIA interpretation. It can also been noticed, that in such fast transients, the transition from brittle to ductile behaviour occurs at higher temperature than in steady-state conditions (~ 1200 C), so the brittle fuel fracture is observed even at high temperatures, and probably up to the melting point.

In the more inner zones, the relative increase of temperature is less important (fig. 2), so the brittle fracture is observed with higher fuel enthalpy. Indeed, the overpressurization in bubbles is given by:
\[ \Delta P_{b,\text{excess}} = P_{b_0} \left( \frac{T}{T_o} - 1 \right) \]

with \( P_{b_0} = \) bubble pressure in EOL
\( T_0, T = \) temperature in EOL and in transient

This explains why the width of the grain-boundary separation zone increases with energy deposition.

In the very inner zones, brittle fracture occurs probably at a temperature of the same order as in the unrestructured zone of LMFBR fuel: \(~2300 - 2400\ K\). So, the ductile behaviour of the fuel in the "cooling phase" of the transient, where the temperatures are slightly higher (fig. 3), erases the fuel aspect induced by brittle behaviour.

3. RIM formation - Behaviour in RIA

At the pellet edge of the PWR fuel, plutonium production is enhanced due to the large resonant peaks in the \( U^{238} \) absorption spectrum in the epi-thermal energy region. Subsequently, the fission density is increased and the radial power profile modified, leading to about a factor two between the edge burnup and the mean burnup (above about 40 GWd/tU).

High burnup (locally \( \geq 70 \) GWd/tU), in a low temperature region leads to a typical microstructure characterized by:

- an extremely fine-grained structure, in the sub micrometer range (\(~0.02 \mu \) to \(1 \mu \)m instead of \(10 \mu \)m initially),
- a high gas retention, but in major part (until \(~80 \%) undetectable with the microprobe analysis, that means gas localized in grain boundary bubbles and large porosities,
- an increased porosity (more than \(20 \)\% from some authors, ref 12, 13).

Starting at a mean burnup of about 40 GWd/tU, the rim width at the pellet edge of the PWR fuel can reach about 250 \( \mu \)m at an average burnup of 70 GWd/tU (ref. 14), that means about 12 % of the fuel mass. Such a structure is also observed in LMFBR fuel, in the outer part corresponding to the usually unrestructured zone (\(~30 \)\% of fuel mass), at similar burnups.

Some mechanisms have been postulated for this restructuring (based on the accumulation of defects, fission gas bubbles or fission products). But, obviously, the RIM formation is not yet well understood, and additional experimental observations are necessary. Nevertheless, it seems clear that the condition for the rim formation is a local threshold burnup of \(70 - 80\) GWd/tU, independent of the origin of fissions, and a low irradiation temperature (< \(1100^\circ \text{C}\) in ref. 15). Furthermore, the experimental observations presently available, show (ref. 13, 16):
- sub micrometer grains, from about 1 \( \mu \text{m} \) to less than 0.02 \( \mu \text{m} \), apparently bubble free,
- faceted micrometer-size pores: this faceted appearance is due to the much smaller sizes of the surrounding grains,
- nanometer-size bubbles coating the grain boundaries (clearly observed in ref. 16, but not in ref. 13).

This bi-modal repartition of bubble size is associated with a generally well recognized fission gas retention: an important depletion of matrix fission gas combined with a low gas release and a high porosity, that means the major part of the created fission gases is retained in porosities (> 50 nm).

Based on these experimental observations, some assumptions on the bubbles/pores gas repartition can be made, and the best agreement (ref. 4) with experimental results is obtained with the following hypothesis on the rim description (concerning the 100 \( \mu \text{m} \) thick periphery of a 65 GWd/tU fuel):

- **Small grains** of 0.1 \( \mu \text{m} \) in radius (a).
- **Small intergranular bubbles** \( (r_b \approx 1 \text{ nm}) \) in equilibrium inside the fuel. So, the bubble pressure is given by:

\[
P_b = P_H + \frac{2 \gamma \sin \theta}{r_b}
\]  

and, from the bubble equation of state (Van der Waals), the intergranular gas concentration \( C_g \) (cm\(^3\)/g) is correlated to the intergranular swelling \( (g) \) by:

\[
C_g = \frac{V_M}{\rho N k} \frac{P_b}{g} \frac{T + \frac{B}{k} P_b}{P_H}
\]

\((V_M: \text{molar volume, } N: \text{Avogadro's number, } k: \text{Constant of Boltzman, } B: \text{Van Der Walls constant for Xenon}).\)

Considering the maximum value for \( g \left( \frac{\Pi \alpha}{2a} r_b \text{ or 0.00589} \right) \), a value of 0.005 can be assumed, leading to an intergranular gas retention of 0.191 cm\(^3\)/g \((P_H = 14 \text{ MPa, } T = 700 \text{ K}).\) \((\alpha \text{ and } \theta \text{ are the form factor parameters for lenticular bubbles}).\)

- **Large pores** (~ 1 \( \mu \text{m} \)) overpressurized. The pressure desequilibrium can result from the insufficient number of vacancies to allow pores to grow and reach their equilibrium size. For a porosity of ~ 20 % the major part of gas retention is due to these micrometer-size pores. With a gas retention in the RIM of around 2 to
2.5 cm³/g, and a swelling of 0.195, we obtain a pressure in the pores around 50 MPa, compared with a plenum pressure of 14 MPa.

The characteristics of this bubble/pores repartition are summarized in table 3. These numbers allow to evaluate the stored energy from fission gases, given by:

\[ E_{\text{tot}} = \frac{3}{2\rho} \sum P_{\text{bi}} g_i \]  

We can see that the contribution of small intergranular bubbles and large pores is of the same order.

**Transient behaviour:**

The contribution of the fission gases of the RIM region to PCMI loading during the heating phase implies two phenomena:

- the fragmentation mechanism which is the condition for a fast liberation of the energy stored in intergranular bubbles,
- the clad straining which implies that sufficient energy is available for an effective contribution of fission gases.

The grain boundary separation takes place during the fast heat-up when the overpressurization of intergranular bubbles exceeds the fracture stress of the fuel. Besides, the brittle strength of the fuel decreases as porosity increases (ref. 17), and an increase of porosity of UO₂ from 5 to 20 % can cause a 80 % reduction in fracture strength. So, it is possible to obtain a grain boundary cracking in the rim region at low temperature (~ 900 - 1000°K).

Finally, the work of expanding gas is transformed into clad deformation (figure 1). If \( \sigma_r \) is the rupture stress of the clad, equivalent to an internal hydrostatic pressure \( (2e_g/d_g)\sigma_r \), and \( \varepsilon_r \), the corresponding rupture strain, the condition for clad rupture is roughly given by:

\[ m_L \times \frac{1}{\rho} \sum_i \frac{P_{\text{bi}} g_i \ Log \left( \frac{P_{\text{bi}}}{P_r} \right)}{P_r} \times \frac{2e_g}{d_g} \times 2 \Pi r_g^2 \int_{\varepsilon_0}^{\varepsilon_r} \sigma_d \varepsilon \]  

where
- \( m_L \) is the linear mass of the fragmented zone
- \( \varepsilon_{\text{th}} \) is the contribution of differential thermal dilatation (fuel-clad) to clad deformation
- \( P_{\text{bi}} \) is the pressure of the bubble group \( i \), at fragmentation temperature (or at a later time during the heat-up phase)
- \( e_g, d_g \) thickness and mean diameter of the clad

In case of fully elastic behaviour of the clad (no plastic deformation), the second term has the simplified expression:

\[ \Pi e_g r_g \left[ \sigma_r \varepsilon_r - E\varepsilon_{\text{th}}^2 \right] \]  

\[ 501 \]
where \( E \) is the Young modulus.

So the necessary condition for an effective contribution of fission gases to clad loading is that the left term of the inequality (5) must be positive. Assuming as rupture conditions \( T_{\text{rim}} \approx 900^\circ \text{K} \) (similar to REP-Na1 test of CABRI REP), and either \( P_r = 80 \) MPa (usual clad laws) or \( P_r = 30 \) MPa (degradation of clad mechanical properties), an evaluation of the gas expansion work after grain boundary cracking is made (Table 3). We conclude that significant energy is available for clad loading, but the occurrence of a clad failure will depend on the clad mechanical properties, rather unknown in the field of high strain rate and highly corroded cladding.

From a similar bubbles/pores gas repartition in the fuel and classical mechanical laws, the interpretation of REP-Na1 with SCANAIR code, give also results which can be considered as rather good (ref. 10).

### 4. Fission gas release

In the NSRR and CABRI tests, high gas release has been observed with commercial fuel rods refabricated or not, with burnup \( \geq 40 \) GWd/tU and with low energy deposition (Table 2). At these enthalpy levels, diffusion phenomena are theoretically not or only slightly activated.

This is confirmed by the microprobe examinations on REP-Na1, which show no significant change before and after test (ref. 11). A strong contribution of the rim region must be envisaged, but even for very high burnup fuel \( \sim 65 \) GWd/tU), all the rim gas represents only \( \sim 15 \) % of the retained gas. For REP-Na3 and HBO fuel, it is only 7.5 %, and in the case of GK1 fuel \( 42 \) GWd/tU), the rim is just incipient. So, we can conclude that the gas release is issued in major part from porosity and grain-boundary bubbles.

The intergranular gas concentration is limited by the specific surface of the grains. Assuming lenticular form and equilibrium conditions inside the fuel, an upper value can be obtained, depending on grain size, temperature and bubble radius, given by the expression:

\[
C_{g_{\text{e}}_{\max}} = \frac{\Pi \alpha V_M P_b}{2 \rho N_k a} \left( \frac{P_b}{T + \frac{B}{k} P_b} \right)
\]

with the bubble pressure \( P_b \) given by the equation (2).

The evolution of \( C_{g_{\text{e}}_{\max}} \) versus bubble radius is given in figure 6, in the conditions of a PWR fuel \( \sim 40 \) GWd/tU. In the range of bubble sizes observed in a PWR fuel \( \sim 50 \) nm only in the inner zones, but much lower : 1 to 10 nm elsewhere), the maximum intergranular gas content is relatively low \( \sim 1 \) % of retained gas). And even with the assumption of an overpressurization of the intergranular bubbles in EOL state, which cannot exceed the fuel rupture stress and which is significant only for large bubbles, the capability of gas accumulation in grain face bubbles is still low \( \leq 2 \) %.

So, by elimination we conclude that the released gas results mainly from the gas accumulated in porosities in EOL : as fabricated porosities in which some fission gases have been released or corner pores formed during irradiation. This assumption is coherent
with the microprobe results which show that the difference between the integral and the gas formation increases with burnup, until 25% at 65 GWd/tU in some cases.

In order to estimate the fission gas quantity in porosities, we assume that the porosities are in equilibrium with the plenum, so given by the equation (3), where $P_b$ is the plenum pressure. This leads to a significant quantity of gases as seen on figure 6 (left side, more than 10% of retained gas), and could explain the high gas release measured in NSRR and REP-Na3 tests (Table 2).

However, this assumption is difficult to justify theoretically, considering the low diffusivity of fission gases at operating temperature of a PWR fuel. It is probably totally wrong at low burnup for which porosities are most probably filled with sintering gases or Helium which is relatively mobile during normal PWR operating conditions. But, it seems also realistic to assume that the fission gas quantity increases in porosities with temperature and burnup. So satisfactory agreement with experimental results can be obtained at high burnup fuel, assuming porosities filled with fission gases in equilibrium with plenum.

The gas release kinetics are not presently clearly identified. In fast transients, the determinant step for fission gas release to the plenum, is the gas transfer inside the more or less interconnected porosity network. So, the effective gas release is correlated to the structural fuel evolution. And, even if some gas release can take place during the heating phase mainly from porosities, major part occurs probably during the cooling phase, by fuel cracking and stress relaxation (after the PCMI phase). In RIA transient, local fission gas release occurs first in the outer zones, from grain boundary fragmentation (intergranular gas release) and subsequently from intragranular migration if the thermal level is sufficient. Significant and rapid modification of the radial temperature profile leads to cracking and micro structural changes. Quick cool down induces radial micro-cracks in the peripheral region, making easier gas transfer to the closed gap (PCMI phase). So, this last one becomes the main path for gas escape to the plenum, but nevertheless, it will take a significant time.

The delays between local gas release, gas release in the gap, and escape to the plenum are of importance:

- The gas released from the grains, but still present inside the fuel can contribute to PCMI.
- The fission gases accumulated in the gap induces a temporary pollution and degraded fuel-clad thermal exchanges can affect significantly fuel and clad thermal evolutions.

So, these different steps must be modelled in the computer codes, but require experimental data which are not presently available.

5. Fuel swelling

High potential for fuel swelling results from important retention of fission gases (Fig. 5). Nevertheless, taking into account the nature of the mechanisms involved: vacancies diffusion and plastic creep, a significant level of temperature is theoretically necessary to activate the phenomena, depending on the transient duration.
In the inner zones where, during the post transient phase, the fuel stays a significant time in a hot state (~ 3 sec), the threshold is ~ 1400- 1500°C, but ~ 500°C more in the fuel outer zones where quick cooling occurs. So fission gas induced swelling cannot contribute to the plastic clad deformation in case of low energy deposition (roughly, maximum fuel enthalpy < 100 cal/g). In particular, the important residual clad deformation (~ 2 %) obtained in GK1 test, at 89 cal/g (Table 2), is difficult to explain. The fuel thermal expansion is also too low. It contributes only to some tenth per cent to clad deformation.

Even, taking into account the different sources of uncertainties: energy deposition, gap pollution which can increase the fuel temperatures (calculated with the SCANAIR code), the fission gas induced swelling is too low.

Some estimations have been made in order to identify the origin of fuel swelling:

- porosities and intergranular gases: taking into account the yield stress of the clad, significant swelling implies a high gas concentration outside the fuel matrix (40 to 70 %), that means important intragranular migration. This feature is inconsistent with the microprobe results of REPNa1, which show no significant intragranular migration with ~ 25 % energy deposition more, and with the measured gas release in GK1.

- Intragranular gas: in EOL, the fission gas is mainly in a confined form: solution or microbubbles, with a volume probably close to the atomic volume. Coalescence phenomena and bubble growth can lead to significant swelling, with nevertheless limited bubble size, undetectable by optical microscopy. This assumption seems presently the most likely one, even if theoretically the mechanisms of vacancy diffusion are not activated at the calculated temperature levels (< 1200°C).

To explain enhanced diffusion coefficients, stoichiometry considerations can be made (ref 18). With burn-up increase, the part of fissions of U-235 decreases and the part of fission coming from Pu-239 increases. The fission product yields for Pu are known to bind less oxygen compared to U-235 fission. Hyperstoichiometry would increase about five to seven times faster for Pu-239 fission then for U-235 fission. This is coherent with the observation of the higher oxide phase $U_4O_9$ in the outer part of the high burn-up fuel. Diffusion in $UO_{2+X}$ is known to increase greatly with x. In addition, hyperstoichiometry has also a strong impact on creep behaviour and fuel thermal conductivity, which is degraded. So, these different effects on fuel material properties could enhance intragranular fuel swelling and also fission gas release.

Finally, the proportion of Pu-239 fissions increases more rapidly in fuel with low initial enrichment. So, if stoichiometry effect exists, it should be more important in fuel with the lower enrichment. This interpretation is coherent with the actual results of the SCANAIR code, which show, with uncorrected diffusion coefficients: an underestimation of clad deformation in GK1 (3.4 % enrichment), and also in REPNa3 (4.5 %) but less important, and an overestimation in REPNa2 (6.85 %).
CONCLUSION

The potential for swelling and transient expansion work under rapid heating conditions characterises the high burn-up fuel behaviour by comparison to fresh fuel. This effect is resulting from the steadily increasing amount of gaseous and volatile fission products retained inside the fuel structure.

Despite long experience and investigations a precise knowledge on the mode and form of gas retention inside the fuel has still not yet been reached. In particular porosity gas seems to play a key role and can contribute significantly to clad failure under the conditions of the reactivity initiated accident especially when corrosion effects reduce the ductility of the cladding and so the ability to accommodate the transient volume change.

The present attempt to quantify the gas behaviour has been largely motivated by the results from the global tests both in CABRI and in NSRR. A coherent understanding of specific results, either transient release or post transient residual retention has been reached.

The early failure of REPNa-1 with consideration given to the satisfactory behaviour of the father rod of the test pin at the end of the irradiation (under load follow conditions) is to be explained both by the transient loading from gas driven fuel swelling and from the reduced clad resistance due to hydriding.

This demonstrates that the transient load potential resulting from the fission products must be understood in detail at the same level as the transient clad mechanical properties at high burn-up must be known in order to establish mechanistic failure criteria and to situate the technological limit of the fuel pin design.

Analytical experiments in the field of fission gas behaviour are extremely difficult to perform, however, as presented in this paper, a variety of experimental results, covering the whole range of parameters give sufficient hints for coherent understanding.

One major parameter which influences the gas effects is the system pressure. The NSRR and CABRI experiments do not allow presently to simulate the reactor conditions in this field.
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### TABLE 1 - Experimental programs on irradiated LWR fuel behaviour in RIA transients

<table>
<thead>
<tr>
<th>Experimental program</th>
<th>SPERT (Ref. 1)</th>
<th>NSRR (Ref. 2)</th>
<th>CABRI-REP (Ref. 3) (1st phase-REP Na)</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Type of fuel</strong></td>
<td>BWR (7% enriched UO$_2$)</td>
<td>PWR (10-20% enriched UO$_2$)</td>
<td>PWR (4.5-7% enriched UO$_2$ - MOX fuel)</td>
</tr>
<tr>
<td><strong>State of fuel</strong></td>
<td>Scliced-down version of standard BWR type fuel rods (high linear rating)</td>
<td>Short-sized standard PWR fuel rods. P = 0.1 MPa</td>
<td>Integral or segmented pin. Long segments of refabricated commercial pins.</td>
</tr>
<tr>
<td><strong>Initial state</strong></td>
<td>Room temperature</td>
<td>* Room temperature</td>
<td>* Isotherm at 280°C</td>
</tr>
<tr>
<td>(temperature / pressure)</td>
<td>* Same internal pressure as EOL</td>
<td>* Stagnant water (.1 MPa)</td>
<td>* Internal P (.1 to .2MPa)</td>
</tr>
<tr>
<td><strong>Heating rate</strong></td>
<td>~ 4 x 10$^5$ K/s</td>
<td>3 x 10$^5$ K/s</td>
<td>* External P (~.5 Mpa)</td>
</tr>
<tr>
<td><strong>Radial restraint</strong></td>
<td>Clad</td>
<td>Clad (open gap)</td>
<td>* Flowing sodium 1-2 x 10$^5$ K/s</td>
</tr>
<tr>
<td><strong>Axial restraint</strong></td>
<td>Yes</td>
<td>Yes</td>
<td>Clad</td>
</tr>
<tr>
<td><strong>Type of diagnostic in transient</strong></td>
<td>* Fuel-clad growth</td>
<td>* Fuel-clad elongation</td>
<td>* Microphones</td>
</tr>
<tr>
<td></td>
<td>* Capsule pressure</td>
<td>* Coolant and clad surface thermocouples</td>
<td>* Flowmeter</td>
</tr>
<tr>
<td></td>
<td></td>
<td>* Pressure transducers in pin (except in JM series) and in capsule</td>
<td>* Pressure transducers</td>
</tr>
<tr>
<td><strong>Main results</strong></td>
<td>2 clad failure at low enthalpy: 147cal/g for 3.5GWd/t 85cal/g for 32GWd/t but no failure at 170 cal/g for 13GWd/t ⇒ large scattering in the results</td>
<td>Extensive microcracking in outer zones at low enthalpy-fuel swelling in inner zones at higher enthalpy</td>
<td>* No clad failure for mean burnup fuel (&lt;33GWd/tM) at 200cal/g.</td>
</tr>
<tr>
<td></td>
<td></td>
<td>clad failure at ~150-160cal/g except in 20% enriched rod</td>
<td>* Clad rupture for high burnup fuel at ~30cal/g and fuel ejection</td>
</tr>
<tr>
<td><strong>Reactor case representativity</strong></td>
<td>?</td>
<td>very limited (irradiation, conditions coolant)</td>
<td>Limited (pressure, nature of coolant)</td>
</tr>
<tr>
<td>FACILITY</td>
<td>CABRI-REP</td>
<td>NSRR</td>
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<td></td>
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<tr>
<td></td>
<td>REPNa1</td>
<td>REPNa2</td>
<td>REPNa3</td>
</tr>
<tr>
<td>Enrichment (%)</td>
<td>4.5</td>
<td>6.85</td>
<td>4.5</td>
</tr>
<tr>
<td>Burn-up (Gwd/t)</td>
<td>63.8</td>
<td>33.2</td>
<td>52.8</td>
</tr>
<tr>
<td>Corrosion thickness (μ)</td>
<td>80</td>
<td>4</td>
<td>40</td>
</tr>
<tr>
<td>Internal pressure (b) (P_{int} - P_{channel})</td>
<td>0</td>
<td>0</td>
<td>2</td>
</tr>
<tr>
<td>Peak fuel enthalpy (cal/g)</td>
<td>116</td>
<td>215</td>
<td>123</td>
</tr>
<tr>
<td>Axial maximum clad deformation (%)</td>
<td>early failure (at 30 cal/g)</td>
<td>3.5</td>
<td>2.1</td>
</tr>
<tr>
<td>Gas release (%)</td>
<td>5.5</td>
<td>13.4</td>
<td>12.2</td>
</tr>
</tbody>
</table>

* Provisional values - To be confirmed
TABLE 3 - Bubble/pore repartition in rim zone for a 65 GWd/tU burnup and consequences on transient behaviour assuming fuel fragmentation at 900°K

<table>
<thead>
<tr>
<th>State*</th>
<th>Intergranular bubbles</th>
<th>Large pores</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Radius</td>
<td></td>
</tr>
<tr>
<td>E</td>
<td></td>
<td>1 nm</td>
</tr>
<tr>
<td>O</td>
<td>Swelling</td>
<td>≥ 1 μ</td>
</tr>
<tr>
<td>L</td>
<td>Gas retention (cm³/g)</td>
<td>.191</td>
</tr>
<tr>
<td></td>
<td>Pressure (MPa)</td>
<td>1240</td>
</tr>
<tr>
<td></td>
<td>Energy (J/g)</td>
<td>.886</td>
</tr>
<tr>
<td></td>
<td>Total energy (J/g)</td>
<td>2.1</td>
</tr>
<tr>
<td></td>
<td>1/ Expansion work (J/g)</td>
<td>2.27</td>
</tr>
<tr>
<td></td>
<td>&quot; &quot; with 5 % fuel mass (J/cm)</td>
<td>.625</td>
</tr>
<tr>
<td></td>
<td>2/ Expansion work (J/g)</td>
<td>3.02</td>
</tr>
<tr>
<td></td>
<td>&quot; &quot; with 5 % fuel mass (J/cm)</td>
<td>.832</td>
</tr>
</tbody>
</table>

* Local fuel conditions: PH = 14 Mpa, T = 700°K

1. Pr = 80 Mpa
2. Pr = 30 Mpa
Fuel and clad behaviour in RIA transients.

**EOL state**

- Fast heating
- High stress field on grain boundaries
- Grain boundary cracking
- Clad failure depending on hydrostatic stress and clad strength
- Grain carrying gas flow (depending on failure type)

State after clad failure

\[ P_b \neq P_{bo} \frac{T}{T_0} \]
Fig. 2: Fuel temperatures radial profiles at PPN at End Of Life and for different radially average Fuel Enthalpies (REP-Na2 case)

Fig. 3: Fuel-clad temperatures evolutions in RIA Influence of coolant (REPNa1 case)
Figure 4a: Typical evolution of center and surface temperatures in a PWR fuel (maximum burnup)

Figure 4b: Radial profile of temperature in fuel
Grains size 5.E-4 cm External pressure 10MPa

**FIG. 6**: Gas concentration in intergranular bubbles and porosities (equilibrium conditions)
1. Introduction

Phenomena of importance to the behaviour of high burnup fuel subjected to conditions of rapid overpower (i.e., LWR RIAs) include the change in cladding material properties due to irradiation, pellet-clad interaction (PCI) and "rim" effects associated with the periphery of high burnup fuel. "Rim" effects are postulated to be caused by changes in fuel morphology at high burnup.

Computer models have been in place in Canada for many years to evaluate the behaviour of CANDU fuel during overpower transients associated with large break loss-of-coolant accidents (LOCAs). The computer code ELOCA (Reference 1) is capable of providing a temperature, stress and fuel morphology based mechanistic assessment of the transient thermal and mechanical behaviour of Zircaloy-4 clad, pelletized UO$_2$ fuel.

Typical discharge burnups for CANDU fuel are low compared to LWRs (Reference 2). Maximum linear ratings for CANDU fuel are higher than those for LWRs. However, under normal operating conditions, the Zircaloy-4 clad of the CANDU fuel is collapsed onto the fuel stack. Thus, the ELOCA code models the transient behaviour of the fuel-to-clad interface and is capable of assessing the potential for pellet-clad mechanical interaction (PCMI) failures for a wide range of overpower conditions.

As part of the code's validation process, ELOCA has been successfully used to model the transient thermal and mechanical behaviour of LWR fuel in selected RIA fuel experiments from the NSRR. The results of these assessments, along with a discussion of the modelling of the phenomena of importance to high burnup fuel behaviour during rapid overpower transients, will be discussed.

2. CANDU Fuel

CANDU fuel elements are fabricated from natural uranium dioxide fuel pellets (97-98 % theoretical density) inside Zircaloy-4 clad (Figure 1). A typical fuel element is approximately 500 mm long and 14 mm in outer diameter. The cladding is approximately 0.4 mm thick. A graphite based lubricant is baked onto the clad inner surface. Zircaloy-4 spacers are brazed to the clad to maintain inter-element spacing, and radial separation between the fuel bundles and the horizontal pressure tube. The fuel elements are back-filled with a helium/argon fill gas at room temperature and atmospheric pressure. The fuel elements are fabricated into bundles by attaching the fuel elements to Zircaloy-4 endplates.

Commercial irradiations employ a once-through fuel cycle based on refuelling at power. Studies have not identified any life limiting phenomena which would preclude the use of the current fuel design to burnups of at least 600 MWh/kgU (25 GWd/t) (Reference 2).

A substantial database has been established for advanced fuel cycles using MOX [(U,Pu)O$_2$, (Th,U)O$_2$ and (Th,Pu)O$_2$] and slightly enriched uranium (SEU) for normal operation, including operational transients, to burnups in excess of 1000 MWh/kgU (40 GWd/t).
As the cladding is collapsible onto the fuel elements, PCMI exists during normal irradiation either by coolant pressure induced clad creep down for low power fuel elements or by differential thermal expansion of the UO\textsubscript{2} with respect to the clad for high power fuel elements. Therefore, any increase in power produces a transient increase in PCMI. Operational constraints are used to avoid power-ramp failures during normal operation.

Figure 2 illustrates a typical overpower transient for a CANDU core for a critical break large LOCA. The overpower transient has a pulse width of approximately 1 second. Licensing considerations preclude centrel ine melting of the fuel. This limits the sum of the initial stored energy in a maximum powered fuel element and the energy in the overpower during the first 5 seconds to less than approximately 960 kJ/kg (230 cal/g). As the highest rated fuel element at 100% full power operates at 63 kW/m \textit{i.e.}, an average stored energy of 343 kJ/kg (82 cal/g), the license limit for the energy in the overpower during the first 5 seconds is equivalent to 617 kJ/kg (147 cal/g).

3. Expected Overpower Fuel Behaviour

The transient thermal and mechanical aspects of fuel behaviour during overpower transients are described as a function of the rate of energy addition to the fuel. The expected behaviour of CANDU fuel during large break LOCA-induced overpower transients is shown to be predominantly affected by transient thermal processes and to be far less severe than the in-reactor fuel response to overpower transients representative of reactivity initiated accidents in light water reactors.

3.1 Fission Heat

The fission process is an energetic nuclear phenomenon. Fissioning of a U\textsuperscript{235} atom releases approximately 200 MeV of energy, of which approximately 86 per cent is the kinetic energy of the two fission fragments and neutrons, approximately 9 per cent is beta/gamma energy and the remainder is neutrino energy. The rate of transformation of fission energy to thermal energy is very fast \textit{i.e.}, less than 10\textsuperscript{-10} seconds (Reference 3). Each fission event adds 3.1x10\textsuperscript{14} kJ to the lattice. As an example, a typical CANDU fuel element, operating at an element linear power of 50 kW/m, produces 7.8x10\textsuperscript{14} fissions/s. Thus, during steady state operation, the fission energy of 41.1 kW/kg, generated in a fuel element operating at 50 kW/m, is transferred to the coolant.

In the Reactivity Initiated Accident (RIA) tests conducted in the Power Burst Facility (PBF), the overpower transients typically have a duration of 0.050 seconds, with a maximum specific fission rate of approximately 5x10\textsuperscript{20} fissions/s/kg (Reference 4). This is slightly lower than the 1.5x10\textsuperscript{21} specific fission rate obtained in the Japanese Nuclear Safety Research Reactor (NSRR) RIA test series with a 0.012 second pulse duration (Reference 5). Since a fission path within the fuel is about 10 microns long by 150 Å in diameter, then approximately 40 per cent of the fuel volume is affected by fission events during a typical RIA overpower pulse.

The fission fragments produce local shock waves (0.1-1 GPa) and local thermal stresses (0.1-1 GPa) in the UO\textsubscript{2} lattice (Reference 3). These stresses dissipate at the speed of sound in UO\textsubscript{2}. At fission rates in excess of 2x10\textsuperscript{21} fissions/s/kg, insufficient time would be available to dissipate these local stresses before the next fission event occurred. At higher fission rates, stresses in the lattice could provide a driving force for mechanical processes \textit{i.e.}, energetic fuel dispersal. At lower fission rates, stresses due to fission events have sufficient time to dissipate and energy transfer processes are thermally controlled.
3.2 Effect of Fission Rate on Fuel Behaviour during Overpower Transients

The fission rate determines the rate of energy deposition in the fuel. The fission rate increases from the fuel centreline to the pellet surface due to flux depression within the fuel pellet. The fission rate can be further enhanced at the fuel pellet surface by the distribution of fissile atoms (i.e., Pu\(^{239}\)) associated with neutron capture by fertile atoms, such as U\(^{238}\) (Figure 3).

Figure 4 illustrates the specific fission densities associated with known "overpower" transients ranging from power ramps to pulsed reactor tests. The overpower transients predicted in the large break LOCA analyses for a CANDU large break LOCA, the OPTRAN test results from the PBF and the CANDU-PBF test have a similar energy deposition rate (References 7 and 8). The NSRR, SPERT-CDC and PBF-RIA tests form another group with energy deposition rates approximately two orders of magnitude more severe (References 4, 5, 9 and 10).

Figure 5 illustrates the thermal response of a fresh fuel element subjected to an NSRR type of overpower transient (i.e., 10 ms pulse width) as predicted by the ELOCA code. The initial fuel radial temperature distribution is uniform at 300 K. As shown in Figure 5, the maximum fuel temperatures initially occur near the pellet surface. These predictions are consistent with observed fuel behaviour in the NSRR tests and are considerably different from the behaviour expected for CANDU fuel during large break LOCA-induced overpower transients.

3.3 Fuel Thermal Response Important to Overpower Transients

Heat transfer from the fuel during overpower transients is governed by the thermal diffusivity, the ratio of thermal conductivity to heat capacity, of the UO\(_2\). The following sections examine the effect of irradiation, porosity, stoichiometry and fission product content on these parameters.

3.3.1 Thermal Conductivity of UO\(_2\)

The thermal conductivity of UO\(_2\) is needed for the assessment of fuel temperatures during normal operating and accident conditions. The thermal conductivity of UO\(_2\) is influenced by the fuel stoichiometry, the presence of plutonium and the extent of irradiation (i.e., pellet cracking, fission product generation and fuel porosity).

It is generally accepted that the UO\(_2\) thermal conductivity can be expressed by the addition of two terms. These terms reflect the theoretical premise that heat is mainly conducted in UO\(_2\) by mechanisms based on phonons and small polarons (Reference 11). At lower temperatures (i.e., less than approximately 1500°C), the phonon-based mechanism is dominant. At higher temperatures (i.e., greater than approximately 1800°C), the polaron-based mechanism is dominant.

The phonon component of UO\(_2\) thermal conductivity is represented as:

\[
\kappa_{\text{phonon}} = (A + BT)^{-1}
\]  

where \(A\) represents the effect of phonon-impurity scattering, \(B\) represents the phonon-phonon (Umklapp) scattering process, and \(T\) is the UO\(_2\) temperature.
The thermal conductivity component due to small polarons has the general expression:

\[
\kappa_{\text{polaron}} = DT e^{-E/kT}
\]  \hspace{1cm} \text{(2)}

where D, E and n are constants related to the electrical conductivity and the electron (hole) mobility (Reference 11), and

\(k\) is the Boltzmann constant.

Several authors (References 11 to 15) have proposed values for all the constants associated with determining the thermal conductivity of UO\(_2\). Figure 7 shows UO\(_2\) thermal conductivity versus temperature as predicted via the information in References 11 to 15, inclusive.

Note that the work of Reference 11 does not calculate the constants through a fitting process to a set of experimentally determined thermal conductivity values. The authors of Reference 11 derive the constant values based on the theoretical development of the thermal conductivity and measurements of related properties such as electrical conductivity. Also note that the work in Reference 15 only proposes an equation for the phonon component of thermal conductivity and the correlation in Reference 15 can be applied beyond UO\(_2\) melting temperatures.

Neutron irradiation generally decreases the thermal conductivity of UO\(_2\) through fission product generation and porosity changes.

### 3.3.1.1 Fission Product Generation

The generation of fission products during irradiation has a two-fold effect on fuel thermal conductivity. First, the production of fission products with high partial pressures at power contributes to the formation of bubbles which modify the existing porosity and can consequently affect the thermal conductivity of the fuel. The second effect is the production of fission products or compounds with low partial pressures that cause modifications to the phonon-impurity scattering contribution to fuel thermal conductivity. A discussion of the effect on fuel thermal conductivity of the presence of fission products with low partial pressures is as follows.

Philipponneau (Reference 16) proposed that the effect of solid fission products on conductivity can be modelled by enhancing the value of the parameter A of the phonon term. This parameter is replaced by:

\[
A' = A + \alpha \tau
\]  \hspace{1cm} \text{(3)}

where \(\tau\) is the fractional burnup in atomic per cent (i.e., 10 atomic per cent is equivalent to \(\tau = 0.1\)), and

\(\alpha\) is a constant.

Philipponneau, using the results of Hartlib (Reference 17), gives a value (adapted for the correlation in Reference 12) in (m.K)/W:

\[
\alpha = 1.205
\]

Figure 8 shows the effect of this correction on the thermal conductivity for CANDU fuel with a burnup
of 0, 225 and 700 MWh/kgU. It is apparent from the figure that the corrected thermal conductivity, for the solid fission product effects, is very small for the range of fuel burnups of interest to CANDU reactors. Recent measurements on irradiated fuel confirm that the effect of irradiation on thermal conductivity is negligible for burnups less than approximately 800 MWh/kgU (Reference 18).

3.3.1.2 Porosity
The fuel porosity is comprised of unsintered fabrication porosity and the bubbles formed by the fission release processes. There are two equations used in the literature to simulate the influence of porosity on the thermal conductivity. The first due to Maxwell-Eucken (Reference 19) is:

\[
\lambda_p = \lambda_{sd} (1-p)/(1+(p(\beta-1)))
\]  

where \(\lambda_p, \lambda_{sd}\) are the conductivities of a porous and fully dense material respectively, \(p\) is the porosity volume fraction, and \(\beta\) is a constant.

The other formulation, due to Loeb (Reference 20) and modified by Ross (Reference 21) for \(\text{UO}_2\) is:

\[
\lambda_p = \lambda_{sd}(1-\alpha p)
\]  

where \(\alpha\) is a temperature dependent parameter.

This modified Loeb equation is used more frequently in the study of the effect of porosity on fuel thermal conductivity and is the formulation used in the ELOCA code.

Theoretically, it is expected that \(\alpha\) will decrease as fuel temperature increases since pores will become better conductors of heat (i.e., gaseous and radiation conduction). Hobson et al., (Reference 22), proposed a linear decrease in pore conductivity with temperature up to 800°C via the expression:

\[
\alpha = 2.58 - 0.00058T
\]  

Brandt et al., (Reference 23), confirmed that this expression agrees well with the available data and that it can be used for fuel temperatures up to 2800°C. Martin (Reference 14) argues that, at high temperatures, pore conductivity will be dominated by radiation and consequently the value of \(\alpha\) should fall off at a higher rate than the first power of the temperature.

The experimental results of MacEwan et al., (Reference 24) indicate that the value of \(\alpha\) depends on the fuel fabrication route (i.e., pellet sintering). Extrapolating their results, reasonable bounds for this parameter, which includes a wide range of pore shape and orientation, are:

\[1 \leq \alpha \leq 3\]

3.3.1.3 Effect of Plutonium Content
Olander concludes that the parameter \(A\), representing the effect of phonon-impurity scattering, is almost independent of the plutonium content (Reference 25). Conversely, he concludes that the results from
experiments, in which the oxygen-to-metal ratio is held constant and the fraction of plutonium is varied, can be fitted by varying the parameter $B$ that stands for the phonon-phonon (Umklapp) scattering process. The equation proposed by Olander (Reference 25) for the parameter $B$ and adopted in MATPRO-11 (Reference 13) is:

$$B' = B (1 + 0.6238 P)$$

where $P$ is the fractional PuO$_2$ weight.

Figure 9 shows the thermal conductivity as function of temperature for $P$ values of 0.0000 and 0.0065.

3.3.2 Specific Heat of UO$_2$

The specific heat capacity of fuel is needed for fuel behaviour calculations during normal operating and accident conditions. As the heat capacity is an extensive material property, the addition of small amounts of fission products and/or other materials that do not modify the structure of the bulk fuel matrix for CANDU fuel will have little effect on the heat capacity. The most commonly used representation for the specific heat capacity is due to Kerrisk and Clifton (Reference 26).

Heat transfer from the fuel during overpower transients is governed by the thermal diffusivity, the ratio of thermal conductivity to heat capacity, of the UO$_2$. The diffusivity decreases with increasing burnup (Reference 28) due to the decrease in thermal conductivity (Figure 8). The effect of burnup on fuel specific heat appears negligible but a major effort worldwide is underway to confirm this observation.

3.4 Fuel Mechanical Response to an Overpower Transient

3.4.1 Plastic Core Formation and Pellet Cracking

At temperatures in excess of approximately 1000°C, UO$_2$ can behave as a plastic material. At temperatures near the melting point, UO$_2$ becomes a "viscous" solid (Reference 29). However, the temperature at which the UO$_2$ exhibits plasticity depends upon the heating rate. The results of fuel irradiations in the NRU reactor, indicate a temperature of plasticity close to 2000°C for fast ramps (i.e., 10 second durations) to power (Reference 30).

The peripheral region of a fuel pellet, operating at temperatures less than the plasticity limit, is subject to brittle fracture in an attempt to reduce tensile stresses. The non-linear temperature profile produces tensile tangential stresses and compressive radial stresses in the fuel pellet. Fracturing of the peripheral fuel region (i.e., outside the plastic region), is predominately by radial cracks. These radial cracks penetrate to the plastic inner zone. The deeper the crack penetration, the more the pellet can expand thermally and the greater the potential for PCMI.

For the very short overpower transients typical of NSRR type tests, the initial peak fuel temperature occurs near the rim of the fuel (Figure 5). Portions of the fuel pellet inboard and outboard of this high temperature region are put into tension which further assists fuel cracking and expansion.
3.4.2 Grain Boundary Gas Bubble Behaviour and Microcracking

The behaviour of grain boundary gas bubbles during overpower transients and the impact of this behaviour on the potential for extensive separation of the fuel grain boundaries (i.e., referred to as fuel microcracking) are discussed. Fuel microcracking reduces the thermal conductivity of the fuel. The gas filled space of the microcrack has a lower thermal conductivity than the adjacent UO$_2$. This effect on temperature appears to be negligible when the sheath tightly constrains the fuel (Reference 31) as it would for CANDU fuel during normal operation or during fast heatup transients. Most microcracks are distributed as porosity and their effect on the thermal conductivity would be treated as such.

Due to surface tension effects, grain boundary bubbles tend to be lenticular in shape with the long axis of the bubble oriented along the grain boundary. The dynamics of these gas filled bubbles is dependent on temperature and heating rate (References 32 to 34). Grain boundary bubbles would be expected to grow by vacancy diffusion in regions where the UO$_2$ is sufficiently plastic. In the colder regions of the pellet where the fuel is brittle, volume swelling of the grain boundary bubbles by overpressurization can be by crack propagation from the sharp ends of the lenticular bubble (References 32 and 34) or early interconnection of bubbles by rapid grain boundary vacancy diffusion (Reference 32). At very high temperatures (i.e., greater than 2300°C), grain boundary bubbles can take the form of large spheroid gas pools (Reference 34).

The overpressurization occurs at heating rates where gas atoms arrive at the grain boundary faster than the bubbles can grow to accommodate the arriving gas atoms. At very high heating rates (i.e., greater than 5000°C/s), energetic microcracking (i.e., "explosive fragmentation") has been postulated (Reference 33).

If low temperature, low power fuel is suddenly subjected to an extreme heating rate such as in the RIA tests in NSRR (Reference 5), then an essentially flat temperature distribution with a sharp temperature peak near the pellet perimeter exists for a short time at the peak of the power pulse (i.e., approximately 10 ms elapsed time) (Figure 5). This temperature distribution causes the entire fuel pellet within the temperature peak to be in radial tension and the outboard rim of the fuel to be in tension in the tangential direction. As the fuel-to-sheath contact pressure is increasing rapidly at the same time, this tensile stress region will only exist for a short time. However, this tensile radial stress (the circumferential stress is also tensile) can assist in separating grain boundaries and releasing volatile grain boundary fission products.

Some fracturing of grain boundaries can also occur under fast cooling (i.e., rewet) due to the thermal stresses generated. Gas filled boundaries would be prone to fracturing.

For the specific fission densities corresponding to a CANDU large break LOCA scenario, extensive UO$_2$ morphological changes are not expected. The OPTRAN and CANDU-PBF test results, with specific fission densities similar to the CANDU large break LOCA overpower transients, support this prediction since no unusual UO$_2$ morphologies are found.

3.4.3 Sheath Strain and Failure

One of the rim effects of concern at high burnup is the reduction of clad ductility and impact strength (Reference 6). Figure 6 illustrates the change in Zircaloy-4 yield stress for the high stress conditions typical of PCMI during overpower transients. Irradiation hardening increases the yield stress of Zircaloy-4 with this effect saturating at high dose levels (Reference 6). However, plastic strain increments as small as 0.3% can lead to "work softening" as irradiation damage is cleared from the lattice by "swathing" of
dislocations (Reference 35 to 37). At high stress levels, Zircaloy-4 exhibits a high stress sensitivity leading to localized strain and failure at stress risers on the clad. The swathing of irradiation damage increases this sensitivity. At elevated temperatures (i.e., >500°C), annealing of the irradiation damage would improve the clad ductility at high stress.

A related alloy, Zircaloy-2, used in the fuel channel structure of CANDU reactors, reaches very high fast fluences (>1 MeV). The change in both the ultimate tensile strength (UTS) and the failure strain saturates, in an exponential manner, by a fluence of 8x10^21 n/m^2. Test results at 170°C and a strain rate of 10^{-3} s^{-1}, show the UTS increasing from 290 MPa to a final value of 525 MPa. For the same test conditions, the failure strain decreases from 40% total elongation to just under 10%.

3.5 Fuel Response to an Overpower Transient

Mathews and Small (Reference 31) have proposed a mechanistically-based understanding of the response of irradiated fuel to temperature ramps. At heating rates less than 1°C/s, fission product atoms migrate to grain boundaries, or are swept up by moving grain boundaries, where they precipitate to form bubbles that can interlink, releasing gas first to intergranular tunnels and then to the fuel element free volume. At higher heating rates there is a tendency for the rapidity of arrival of atoms at the boundary to exceed the rate at which spherical or lenticular bubbles can form, and the boundary cracks, potentially causing fragmentation. At higher temperatures the fuel is plastic enough that bubbles can form and gross swelling occurs. At heating rates over 5000°C/s and at temperatures over 1800°C, Mathews postulates that "explosive fragmentation" can occur, caused by large overpressures on a boundary that is "brittle" (i.e., the heating rate is so fast that little plastic flow can occur). At extreme heating rates and temperatures below 1800°C, no gas release is expected as gas atom migration rates are too slow to result in appreciable quantities arriving at the grain boundaries. This gas-induced fuel fragmentation mechanism would act in addition to any fuel thermal stresses imposed as a consequence of transient, radial temperature distributions.

These proposed fuel behaviour mechanisms can be compared to the observations of irradiated fuel behaviour in the NSRR and other RIA tests. In these tests the reactivity insertion is complete within 10 to 100 milliseconds, causing outer fuel temperatures to rise by about 2000°C at heatup rates in excess of 200,000°C/s). These conditions are sufficiently extreme as to be within the zone of "explosive fragmentation" as postulated by Mathews. The tests showed some evidence of planar cracking of the UO_2 grain boundaries, particularly in NSRR tests JM-4 and the outer regions of JM-5. JM-5 was exposed to a lower ramp than JM-4. However, no energetic dispersal of fuel fragments occurred. Therefore, it appears that the restraint imposed by the cladding is sufficient to limit the UO_2 response to microcracking. Other tests at higher energy depositions also failed to show this type of explosive fragmentation.

The results of the SPERT-CDC and RIA ST1 tests show evidence of fuel dispersal at higher fuel enthalpies than tested in the NSRR tests. However, the energetic dispersal has been generally associated with once-molten particles, implying that the degree of cracking and dispersal in Mathews' postulated "explosive fragmentation" may not be significant compared to the disruption caused by gas release on fuel melting and potential transfer of energy from molten fuel droplets to the coolant if gross overall melting occurs.

Between 7.5 and 0.3% sheath strain caused by fuel swelling is observed in the JM-4, 5 and 6 tests. High additional gas release is also observed, 10.6 per cent in JM-5 and 8.3 per cent in JM-6. No measurement is available for JM-4. The results of the microstructural examination show significant porosity caused by
gas bubble formation, consistent with the observed fuel swelling. The transient fission product gas releases were consistent with the loss of grain boundary inventory.

CANDU fuel undergoes slower overpower transients (i.e., durations on the order of a second) with maximum temperature rise rates on the order of 1800°C/s. These durations are longer and the heatup rates are considerably less than for the NSRR RIA tests. As a result, CANDU fuel will not exhibit behaviour typical of Mathews "explosive fragmentation" range. Furthermore, the high coolant pressure in a CANDU in conjunction with the normally collapsed fuel clad will greatly increase restraint on the fuel and reduce swelling compared to the NSRR tests, most of which were performed with little or no coolant pressure and free standing clad. The OPTRAN and CANDU-PBF test results support the contention that CANDU fuel behaviour during large break LOCA-induced overpower transients will be much more benign than the observed fuel behaviour in the RIA tests where extremely rapid gas bubble swelling is postulated to have induced fuel fragmentation.

There is evidence from instrumented power ramp tests at centre temperatures up to about 1800°C and at heating rates of up to about 50°C/s, that the rate of release of fission product gases to sites (i.e., grain boundary traps), from which the fission gases can be released, is comparatively slow, and consistent with diffusion-controlled migration (Reference 38). Extensive fuel cracking is not observed in these experiments. Therefore, it appears that where the fuel temperature is high enough that the rate of arrival of gas atoms at the grain boundaries is rapid, the fuel is sufficiently plastic that the fuel can accommodate the stresses without significant cracking, at least under the comparatively lower burnup conditions relevant to CANDU.

4. Conclusions

A number of contributing factors to fuel rod failure during overpower at high burnups (>60 GWd/t) have been identified (References 6 and 39). These phenomena include:

- increased energy deposition in the fuel rim;
- mechanical damage to the fuel from high pressure gas bubbles;
- reduced cladding ductility and impact strength;
- pellet clad mechanical interaction; and
- corrosion (oxidation and/or hydriding).

These phenomena are modelled by Canadian fuel performance codes. Analysis and experiment have not identified any life limiting behaviour which would preclude the use of current the CANDU fuel design to burnups of at least 600 MWh/kgU (25 GWd/t) (Reference 2). Studies in support of advanced fuel cycles would extend our confidence to burnups in excess of 1000 MWh/kgU (40 GWd/t).

Current studies are extending this burnup range by tests on related alloys (e.g., Zircaloy-2), related fuels (e.g., SIMFUEL (Reference 28)), and re-examination of our high burnup fuels database.
References


Figure 1
CANDU Fuel Bundle

END VIEW

Figure 2
SMOKIN Predicted Overpower Transient
Figure 3
Variation of Radial Power Density with Pellet Radius and Burnup

Figure 4
Fuel Self-Heating Rate as a Function of Specific Fission Density
Figure 5
Transient Fuel Radial Temperature Profile
Generated during the Overpower Pulse of a Typical NSRR Test

![Graph of transient fuel radial temperature profile showing the temperature distribution at different times during the overpower pulse.](image)

Figure 6
True Stress/True Strain for Zircaloy-4 Fuel Sheathing at Large Stresses

![Graph showing the true stress/true strain relationship for Zircaloy-4 fuel sheathing at large stresses.](image)
Figure 7
UO₂ Thermal Conductivity as a Function of Fuel Temperature

a) Figure 7a shows the thermal conductivity of UO₂ as a function of fuel temperature. The graph compares different models:
- **MATPRO-9**
- **Martin**
- **Harding et al.**

The graph indicates that the thermal conductivity decreases with increasing temperature. A temperature of 727 K is marked, which is used in the ELESIM/ELOCA model.

b) Figure 7b further compares the models with the addition of:
- **MATPRO-11**
- **Pillai et al.**

The same trend is observed, with thermal conductivity decreasing as temperature increases. The 727 K temperature point is again marked, emphasizing its use in the ELESIM/ELOCA model.
Figure 8
Effect of Burnup on Thermal Conductivity
(from Y. Philipponneau)

Figure 9
UO₂ Thermal Conductivity in MATPRO-11
Figure 10
Specific Heat Capacity for Non-stoichiometric UO$_2$ at 2500 K as a Function of Oxygen-to-Metal Ratio

Figure 11
UO$_{2-x}$ (U$_x$O$_2$) Specific Heat Capacity as a Function of Fuel Temperature
HIGH BURNUP MODELING CHANGES TO NRC FUEL PERFORMANCE CODES THAT IMPACT REACTIVITY INITIATED ACCIDENTS

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ABSTRACT

The U.S. Nuclear Regulatory Commission (NRC), Office of Nuclear Reactor Research (RES), is supporting Pacific Northwest Laboratory (PNL) to update selected models within NRC fuel performance codes that are important to predicting fuel rod behavior at high burnup levels. Changes to fuel behavior models which reflect high burnup effects may impact fuel rod performance during reactivity initiated (RIA) and slower transients. The model changes discussed herein are relevant to both defining the initial conditions for an RIA and behavior during the RIA, and include 1) radial power distribution and fuel thermal conductivity as functions of fuel burnup, 2) the cladding property changes as a function of fast fluence and waterside corrosion and hydriding, and 3) gas bubble buildup and fracturing at grain surfaces as a possible fuel-cladding loading mechanism at high burnups.

The radial power profile at high burnup has been determined from experimental data to be highly peaked at the fuel pellet edge. However, models previously used in NRC codes did not correctly predict the onset and the extent of edge peaking. The radial power distribution model recently proposed by K. Lassmann from the European Institute for Transuranium Elements constitutes an improvement. Comparisons of this model to measured data for plutonium and neodymium radial distributions indicate reasonably good agreement at all burnup levels. The impact of this edge peaking for RIAs is to reduce the fuel central temperatures and increase the fuel edge temperatures, during the initial part of the transient.

The fuel thermal conductivity has long been suspected to degrade to some extent with increasing burnup, due to the buildup of rare-earth and gaseous fission products. Thermal diffusivity measurements on unirradiated simulated high-burnup sintered urania fuel pellet material do indicate a significant linear increase in thermal resistivity with increasing burnup. This linear rate of conductivity degradation is consistent with analyses of selected in-reactor tests, in which measured fuel center temperatures have been observed to increase at constant power at high burnup levels in rods with stable gap conductance. The actual effect on operating fuel is still under investigation in several in-reactor experimental programs. However, the existing data support a preliminary degradation model that is based on the simulated-fuel diffusivity results.

(a) Pacific Northwest Laboratory is operated for the U.S. Department of Energy by Battelle Memorial Institute under Contract DE-AC06-76RLO 1830.
Zircaloy cladding at high burnups has been observed to have increased yield strength with decreased ductility. This has been attributed to the effects of irradiation damage (fast fluence) and waterside corrosion (hydriding). Fast fluence has the largest effect earlier in life by increasing the yield and tensile strengths. Hydriding appears to have a dominant effect on ductility after hydride concentrations exceed a given level. This behavior has been modeled in the NRC fuel performance codes based on data from tensile and burst tests reported in the literature with local fuel burnups to 63 GWD/MTU.

However, two problems exist with applying these data in model updates: first, experimentally-applied cladding strain rates are significantly less than those experienced during an RIA; and second, the ductility decreases to near zero at high burnup levels, making fracture mechanics more applicable than normal stress or strain criteria for failure analysis. The latter problem is bounded in the NRC codes by assuming that cladding failure is likely (in RIAs) when the cladding hydrogen content exceeds a given level.

The quantity of fission gas bubbles on grain boundaries is well known to increase with burnup. In addition, these bubbles decrease grain boundary strength and promote microcracking. During an RIA it may be hypothesized that severe grain boundary microcracking occurs, thus releasing grain boundary fission gas and forcing fuel fragments against the cladding, which in turn causes high strain rates and high stresses in the cladding. NRC fuel performance codes in the past have not modeled this effect. In the updated models, it is assumed that for RIAs grain boundary fracturing begins to occur when the prettransient burnup exceeds 40 GWD/MTU; and that the extent of fracturing increases with increasing burnup beyond 40 GWD/MTU. The gas released during grain boundary fracturing is further assumed to expand as an ideal gas for the calculation of loading forces. Fission gas release from a non-failed RIA-tested rodlet with significant pre-test burnup was used to estimate the degree of grain boundary fracturing.

Incorporation of the above model changes into the NRC fuel performance codes will improve the ability to predict steady state and transient fuel behavior at high burnup levels. Specifically, these modeling changes can help explain the observed decrease in the fuel rod failure threshold in the recent RIA tests. However, additional analyses and separate effects testing on the cladding and fuel is needed to better define the role of each.
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INTRODUCTION

The U.S. Nuclear Regulatory Commission (NRC), Office of Nuclear Reactor Research (RES), is supporting Pacific Northwest Laboratory (PNL) to update selected models within NRC fuel performance codes that are important to predicting fuel rod behavior at high burnup levels. The subject of this paper is to address those changes to model high burnup behavior that may impact fuel rod performance during reactivity initiated (RIA) and slower transients. The model changes to be discussed are relevant to both defining the initial conditions for an RIA and behavior during the RIA, and include 1) radial power distribution and fuel thermal conductivity as functions of fuel burnup, 2) the cladding property changes as a function of fast fluence and waterside corrosion/hydriding, and 3) gas bubble buildup and fracturing at grain surfaces as a possible fuel-cladding loading mechanism at high burnups. These subjects are discussed in turn below.

RADIAL POWER DISTRIBUTION AND FUEL THERMAL CONDUCTIVITY

The volumetric heat generation (W/m³) at any point within a fuel pellet is proportional to the effective thermal neutron flux multiplied by the sum of the fissile isotope concentrations times their respective effective fission cross sections. At beginning-of-life, the typically low enrichment of the U-235 isotope (3 to 5%) constitutes the only fissile isotope, and the radial distribution of U-235 is uniform. The thermal neutron flux level is only slightly depressed in the center relative to the edge, resulting in a nearly uniform radial distribution of volumetric heat generation.

As burnup proceeds, U-235 is consumed by fission; but resonance neutron capture by U-238 results in a small buildup of plutonium, including the fissile plutonium isotopes Pu-239 and Pu-241. Because of the large value of the capture cross section at resonance energies, this plutonium buildup occurs preferentially, but not exclusively, at the pellet edge. The plutonium content in the pellet builds asymptotically towards approximately 1% pellet

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average and 3% in the pellet rim. Thus the fissile plutonium concentration at
the rim begins to significantly exceed that in the remainder of the pellet as
burnup accumulates, and the radial distribution of the volumetric heat
generation becomes progressively edge-peaked. The radial distribution of fuel
burnup (in relation to the initial concentration of heavy metal atoms) also
becomes progressively edge-peaked.

Accurate calculations of the evolution of neutron flux distribution and
fissile isotope concentrations within a fuel rod require detailed neutronics
code calculations which account for all the interactions and account for the
specific time-dependent neutronic environment of the rod. However, the
environments and fuel designs in standard light water power reactor cores are
sufficiently similar to permit approximate, one-dimensional one-group
calculations, using effective values for fission and capture cross sections.
The RADAR subroutine (Reference 1) represents one such approximate solution
that contains an exponential distribution function that governs the radial
deposition of the plutonium, and effective cross-sections for the plutonium.

The TUBRNP subroutine authored by K. Lassmann et al. at TUI (Reference
2) improves the original RADAR by modifying the parameters for the plutonium
distribution function and accounting for the plutonium isotopes explicitly.
The result is a somewhat more edge-peaked distribution function at nominal to
high fuel burnups, which is supported by comparison to detailed neutronics
calculations and to detailed electron microprobe data on the distribution of
both plutonium and stable fission products (References 2 and 3). An example
of the plutonium buildup and its consequences is shown in Figures 1 and 2,
which apply to a C-E 14x14 type fuel rod with 4.5% U-235 enrichment. The
radial distributions of heat generation and burnup at 60 GWD/MTU pellet-
average burnup are shown in Figure 1. In Figure 2, the ratios of the edge
(outermost 1%) to volume-average values for heat generation and for burnup are
shown. The edge peaking becomes significant from 20 GWD/MTU burnup onward.

The fuel thermal conductivity in the bulk of the pellet is also impacted
by high burnup, by the reduction in the phonon term due to solid fission
product buildup. A preliminary model for this effect was proposed by Lucuta
(Reference 4) based on laser-flash thermal diffusivity measurements on
simulated high-burnup fuel, simulated by the addition of rare earths. Lucuta
found that the thermal resistivity (inverse of conductivity) in the 100 to
1500°C range increased linearly with simulated burnup, with a rate equal to
0.016 °C-m per watt per atom % burnup. The effect of this degradation on the
conductivity as a function of temperature is shown in Figure 3.

The projected reduction in conductivity in the high burnup rim can be
estimated for the previous example by combining the information in Figures 2
and 3. This reduction is shown in Figure 4; it approaches a factor of 2 at
high burnup, and for a lower-enriched rod would exceed 2.
Calculated RIA fuel and cladding performance are impacted by introducing improved radial distributions of power and burnup, in the following ways:

1) The fuel pellet radial temperature distribution following the RIA will initially be edge-peaked, because it initially is proportional to the radial power distribution.

2) The very high concentration of fission products in the high-burnup pellet rim results in significantly reduced thermal conductivity in that region, due both to the high concentration of solid fission products and to the enhanced porosity caused by the high concentration of gaseous products. Reduction of the unirradiated thermal conductivity by more than a factor of two is probable.

3) Gaseous and volatile fission products are held in pores in the fuel region at very high pressures, especially in the pellet rim. The dramatic temperature increase caused by an RIA may lead to fuel structure shattering and expansion, with consequently large cladding strains and strain rates.

4) The cladding temperature history during an RIA will be impacted by both the radial power peaking and the rim conductivity decrease. The cladding temperature increase will be faster, and the peak cladding temperature will possibly be greater, than when these effects are not taken into account.

**IMPACT OF MECHANICAL PROPERTIES CHANGES AT HIGH BURNUP ON RIA FAILURE THRESHOLD**

Fast neutron fluence and waterside corrosion both lead to changes in the mechanical properties of Zircaloy cladding with increasing burnup. The fast neutron fluence increases yield and tensile strength, and decreases cladding ductility due to intrinsic cladding damage (displacement of atoms). However, the fluence effect on uniform strain appears to saturate when a fluence of $4 \times 10^{25} \text{n/m}^2$ is accumulated (i.e., at burnups exceeding approximately 20 GWD/MTU). The effects of waterside corrosion and the concomitant accumulation of hydrogen in the cladding appear to dominate further changes in mechanical properties when the excess hydrogen concentration (i.e., the hydrogen in excess of the solubility limit at operating temperature) exceeds approximately 200 ppm. This typically occurs at burnups in excess of 40 GWD/MTU.

The mechanical property that has the largest impact on fuel rod failure threshold during an RIA is cladding ductility. This is because fuel expansion and swelling translate almost completely to cladding strain during the extremely short time frame of an RIA. The loss of ductility in Zircaloy at high burnup appears to be proportional to the excess hydrogen concentration. We have used data from several cladding tensile tests on high-exposure cladding samples (References 5 to 11) to develop relationships between cladding strength and cladding uniform strain (at maximum test load) and specimen fluence, excess hydrogen, and test temperature. The cladding samples used to develop this relation had measured oxide thicknesses between 4 and 110 microns, excess hydrogen concentrations up to 620 ppm (average in the
Results of this relationship are shown in Figure 5 for cladding strain as a function of excess hydrogen. This relationship indicates that cladding ductility approaches zero at excess hydrogen levels of 500 to 600 ppm (averaged across the wall). Assuming an average hydrogen pickup fraction of 15% (which is well supported by correlated corrosion/hydrogen data sets on numerous PWR cladding samples), the 500 to 600 ppm excess hydrogen level is achieved when the waterside corrosion layer thickness is in the 90 to 100 micron range, which for PWR rods with standard Zircaloy cladding can be achieved by burnups of 50 to 60 GWd/MTU.

In conclusion, the decrease in cladding ductility due to corrosion at high burnup levels is believed to be a major contributor to the observed decrease in the RIA failure threshold at high burnup. Furthermore, the fuel rod failure mechanism during an RIA becomes brittle fracture at high burnup, and failure potential should be assessed using fracture mechanics. However, there are currently no fracture mechanics type data on high burnup cladding. Therefore, NRC codes currently warn the user when excess hydrogen concentrations exceed 500 ppm that current mechanical models may not be applicable, due to cladding embrittlement.

The expressions for cladding failure strain are proposed to replace the existing routines in MATPRO-11 (Reference 12).

**HIGH-BURNUP PELLET MICROCRACKING DURING AN RIA**

Significant quantities of volatile and gaseous fission products become trapped in high-pressure pores within high-burnup fuel pellets, particularly at grain boundary interfaces. It is speculated that the sudden temperature increase associated with RIAs could cause fuel microcracking along the grain boundaries and that some fuel swelling would occur due to the microcracking, resulting in large stresses and high strain rates in the cladding.

Gross radial cracking was observed at the pellet rim in a 42 GWd/MTU test section taken from a PWR rod. This section was subjected to and RIA with an energy deposition of 112 calories per gram in the JAERI Research Reactor (Reference 13). The rod did not fail, but strong initial PCI was also judged to have occurred in this rod, based on real-time axial elongation measurements. The post-test fission gas release (FGR) measured in rod puncture was 12%, and the permanent diametral strain was 1%. The majority of the FGR and cladding strain was judged to have come from the RIA test, judging from the behavior of sibling rods.

To parametrically estimate the effects of microcracking upon fission gas release and cladding strain, the following prescription was adopted:

1) The degree of grain boundary separation during the RIA is assumed to increase by a constant fraction per GWd/MTU local (ring) burnup, starting at 40 GWd/MTU.
2) The rate of grain boundary separation increase is estimated from the net gas release of the nonfailed RIA-tested JAERI rods.

3) Fuel swelling associated with grain boundary separation, and is taken to be proportional to the fraction of boundaries that are separated (per ring).

4) The fuel swelling is estimated from the total diametral strain of the cladding in the non-failed JAERI rod, subtracting first the thermal expansion of the fuel pellets produced by the RIA.

Calculated radial distributions for the burnup and the volumetric heat generation rate for the 42 GWe/MTU JAERI rod are shown in Figure 6. The corresponding estimated adiabatic radial temperature distribution is shown in Figure 7. The estimated grain boundary separation was 25% for every GWe/MTU above 40, up to 100%; and the resulting estimated radial gas release pattern in the fuel pellet is shown in Figure 8. The calculated total release is close to the measured value of 12%. The corresponding estimated radial strain was close to the measured value of 1%.

Alternatively, one could follow the release logic inherent in, for example, the Forsberg/Massih FGR model (Reference 14) and assume that grain boundary separation occurs only where the pre-transient grain boundary inventory exceeds the grain boundary saturation value (calculated from the in-transient temperatures from Figure 7). Then an entirely different radial release pattern is predicted, as shown for example in Figure 9. Utilizing these two release scenarios, the grain boundary separation and gas release can either occur at the edge or well into the interior of the fuel.

These examples illustrate the need for detailed radial scanning with scanning electron microscopy, electron microprobe, and X-ray fluorescence (or equivalent total-gas detection method) on transverse sections from RIA-tested non-failed high-burnup rods, to elucidate the radial release patterns and the corresponding physical state of the fuel, and hence the probable mechanisms causing the fission gas release and the cladding strain during RIA-type transients.

OTHER FACTORS INFLUENCING RIA TEST RESULTS AND ANALYSIS

A sensitivity study on factors influencing RIA transient performance for high-burnup rods (in particular pulse width and initial gap thickness), was completed by Siefken (Reference 15). The FRAPCON-2 code (Reference 16) was used to generate the burnup-dependent state of the rod at the start of the RIA, and the FRAP-T6 code (Reference 17) was used to calculate the transient rod behavior. The study was performed for 17x17 PWR rods, operated to 50 GWe/MTU rod-average burnup. "Hot standby" reactor conditions were assumed for the start of the RIA.

The selected RIA event for this study involved an total energy deposition of 107 cal/g of UO₂; the power pulse width was varied as part of the study. The volumetric heat generation rate at the outer surface of the pellets was assumed to be 2.6 times the volume-average value, due the buildup

539
of plutonium at the pellet rim, and the depletion of the initial U-235. The fuel thermal conductivity in the outer 0.1 mm of the fuel radius was reduced by 50%, due to the assumed combined effects of enhanced local burnup and localized porosity increase in that region, as commonly observed in high-burnup fuel pellets.

The fuel rod behavior was calculated for power pulses varying in width from 10 msec to 100 msec and for initial fuel-cladding radial gap sizes varying from 0.0 mm to 0.05 mm. These ranges in are expected to bracket the probable values for these parameters. These cases with a fuel-cladding gap size of 0.05 mm correspond with a fuel rod that was at a maximum linear power of 40 kW/m before the shutdown preceding the RIA. The cases with a zero fuel-cladding gap correspond with a fuel rod which operated at normal power prior to the RIA, such that the fuel-cladding gap was closed due the cladding creepdown.

The results of the sensitivity study are presented in Table 1. The maximum increase in fuel enthalpy was about 10% less for the 100 msec pulse width cases than for the 10 msec pulse width cases. The maximum temperature of the fuel ranged from 1874 K for Case 3 with a 100 msec pulse and 0.0 mm initial fuel-cladding gap, to 2266 K for the Case 2 with a 10 msec pulse width and a 0.05 mm initial fuel-cladding gap size. The maximum cladding temperature varied from 667 K for Case 4 to 835 K for Case 1. The maximum calculated cladding stress varied from 125 MPa for Case 4 to 565 MPa for case 1. The maximum cladding hoop strain rates ranged from 0.06 per second for Case 4 to 1.2 per second for Case 1. The maximum cladding plastic hoop strain ranged from 0.0 to 1.3%.

Five conclusions on fuel rod behavior during an RIA can be drawn from the results of this sensitivity study. First, the size of the fuel-cladding gap at the start of the RIA has a large influence on the cladding stresses and strains during the RIA. Second, the maximum cladding hoop stress is about 20% less for a power pulse of 100 msec than for one of 10 msec. Third, the maximum cladding temperatures are significantly less for a power pulse of 100 msec than for a power pulse of 10 msec. Fourth, the maximum hoop strain due to pellet-cladding mechanical interaction is calculated to be less than 2% for a 100 cal/g power pulse. Fifth, the maximum cladding strain rates for a 10 msec power pulse are very large, and are outside the regime for which irradiated cladding mechanical properties are available.
SUMMARY

High-burnup effects that should be incorporated in RIA analyses of high burnup rods are:

1. The progressive edge-peaking of the radial distributions of burnup, produced gas, and volumetric heat generation.

2. The burnup-dependent degradation of the fuel thermal conductivity, especially in the high-burnup fuel rim.

3. Cladding waterside corrosion at high burnup, that causes reduction of cladding ductility due to the accumulation of excess hydrogen in the cladding leading to hydride formation.

4. The added FGR and cladding strain, speculated to be due to fuel microcracking.

The last effect listed above appears probable, based on observations to-date; however more test and examination data are needed on high burnup rods in order to confirm it. Rod puncture data, pre/post transient diametral data, and fuel transverse sections are needed from nonfailed, RIA-tested high-burnup rods. Furthermore, detailed radial scans are needed on the cross sections, by electron microprobe and X-Ray fluorescence (or equivalent), to confirm the distributions of produced gas, gas retained in the matrix and gas retained in porosity. In order to elucidate the fission gas release mechanisms, these data need to be supplemented by detailed radial scanning electron microscopy to confirm the physical state of the fuel in the separate radial regions defined by differing retained gas fractions.

The increased hydride levels in high exposure cladding appears to be a major reason for reduced RIA failure thresholds for high burnup rods. Fracture mechanics data are needed on the impact strength of irradiated, hydrided Zircaloy cladding at operating temperatures, in order to predict RIA failure thresholds. (In the interest of postirradiation fuel handling, impact strength data are also needed at room temperature.)

A sensitivity study has examined the effect of fuel rod and operational parameters on calculated fuel performance during RIAs at high burnup, using the NRC codes FRAPCON-2 and FRAP-T6. This study has shown that pulse width and pellet-to-cladding gap size had dominant impacts on the calculated fuel behavior. The influence of the fuel-cladding gap demonstrates the importance of pretransient rod thermal and mechanical conditions and properties for the prediction of transient fuel rod behavior; these will be even more crucial when reduced cladding ductility is factored into the calculations.

Therefore in order for the results of RIA tests on high-burnup rods to be applicable to projected RIAs in commercial plants, the test rod characteristics must approximate those of high burnup power reactor rods; the test conditions must be sufficiently prototypic; and the analysis tools (performance codes) must contain sufficient capabilities and models to simulate both the rods and the tests. Specific in-reactor data, ex-reactor data, and code improvements are still needed to achieve this goal.


342

### Table 1
Predicted Behavior of Fuel Rods During an RIA

<table>
<thead>
<tr>
<th>Characteristic of Fuel Rod Behavior</th>
<th>Pulse Width, msec</th>
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</thead>
<tbody>
<tr>
<td></td>
<td>10</td>
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<tr>
<td></td>
<td>Gap size, mm</td>
</tr>
<tr>
<td></td>
<td>0.0</td>
</tr>
<tr>
<td>Ratio of radially averaged enthalpy increase to energy deposition</td>
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</tr>
<tr>
<td>Maximum Fuel Temperature, K</td>
<td>2034</td>
</tr>
<tr>
<td>Location of maximum fuel temperature (fractional radius)</td>
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</tr>
<tr>
<td>Maximum cladding surface temperature, K</td>
<td>835</td>
</tr>
<tr>
<td>Maximum hoop stress in cladding (MPa)</td>
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</tr>
<tr>
<td>Maximum cladding hoop strain, %</td>
<td>1.3</td>
</tr>
<tr>
<td>Maximum cladding hoop strain rate, per second</td>
<td>1.2</td>
</tr>
</tbody>
</table>
Figure 1. Typical High-Burnup Radial Distributions of Burnup and Heat Generation (4.5% Enriched Urania Pellet at 60 Gwd/MTU)

Figure 2. Ratios of Burnup and of Heat Generation in Outermost 1% of Pellet to Volume-Average Values, as Functions of Volume-Average Burnup
Figure 3. MATPRO-11 Urania Fuel Thermal Conductivity (at 95% Theoretical Density) as a Function of Temperature, with Burnup Degradation as Suggested by Lucuta.

Figure 4. Ratio of Unirradiated Urania Thermal Conductivity in Pellet Rim Region (Temperature = 450 °C) to Burnup-Degraded Conductivity, as a Function of Volume-Average Burnup.
Figure 5. Engineering Uniform Strain (at Maximum Load) as a Function of Excess Hydrogen and Temperature at a Fast Neutron Fluence of $11 \times 10^{25}$ n/m$^2$. (Calculated with new PNL mechanical models).

Figure 6. Radial Distributions of Burnup and Heat Generation for an RIA-Tested PWR Rodlet (3.4% Enriched, at 42 Gwd/MTU).
Figure 7. Calculated Adiabatic Temperature Distribution for an RIA-Tested Non-Failed PWR Rodlet (energy deposition of 112 Calories per Gram).

Figure 8. Calculated Radial Distribution of Fission Gas Release in the RIA-Tested Rodlet (assuming 100% Release where localized burnup exceeds 44 Gwd/MTU).
Figure 9. Calculated Radial Distribution of Fission Gas Release in the RIA-Tested Rodlet (assuming release of grain boundary and re-solved gas where the grain boundary concentration exceeds the saturation concentration).
The Project to implement into CABRI a Pressurized Water Loop. Motivations and objective of the future test program.

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I. INTRODUCTION

Three major aspects represent the purpose of the present paper:

- Identify the arguments which show the need of a pressurised water-loop in CABRI.
- Review the performance and diagnostic requirements in the perspective of a future RIA test program.
- Present the outcome of the technological feasibility study.

a) The arguments:

The recent high burn-up fuel tests in NSRR and in CABRI have invalidated the former fuel design acceptance criteria. In particular, there is no longer any doubt that with increasing burn-up, the transient mean fuel-enthalpy failure and dispersal of the fuel decreases significantly.

The physical phenomena must be understood and modelled: post-failure phenomena must be studied and evaluated under the conditions of a representative environment. In fact, the efficiency of energy transfer during fragmentation and dispersal could be high for high burn-up fuel.
The presently on-going experimental effort is important but insufficient. A test facility which reproduces more precisely the reactor conditions: pressure, flow and temperature of the coolant water, is needed. The major reason for the request of representativity is to be seen as the risk to enter into a sufficiently long lasting departure from nucleate boiling. The associated rapid clad temperature increase initiates a new threat for loss of clad integrity. The system pressure is, in this sequence of phenomena, a key parameter which cannot be simulated. Furthermore, the transient clad-to-coolant heat transfer and the fission gas behaviour can only be studied coherently in this representative environment.

b) Performance and diagnostics

The facility must allow to reach maximum mean fuel enthalpies well beyond the values calculated by the last generation neutronic codes. The enthalpy to DNB must be reached if this level cannot be excluded apriori, a cliff edge behaviour is to be expected beyond the occurrence of this level.

Flow must be measured with precision in the straight and the reversed direction and failure time and location diagnostics must be provided. Especially DNB occurrence and duration must be detected and recorded.

Potential post-failure events (FCI) must not only be securely confined but also precisely measured.

c) The feasability

The implantation of the pressurized water loop into CABRI is possible, this is the result of a detailed study which will be presented. The presently available performance limits and diagnostic capabilities will greatly be conserved.

In the following paragraphs, we aim to give some detailed insight into this well advanced project and try to transmit our conviction that the upgraded and transformed CABRI-PW-test facility would represent the experimental set up for the determination of the fuel performance limits under RIA conditions, for evaluating the performance of improved and advanced design concepts and, last but not least, for the global validation of the relevant computer codes.

II. THE EXPERIENCE OF TEST PERFORMANCE IN CABRI

Since 1978, around 60 experiments have been carried out in CABRI. They concern mainly studies on fast breeder fuel (more than 50 tests) and partly on PWR fuel (5 tests in sodium, up to now).

The present experimental test program on PWR rods under RIA conditions, with sodium as coolant, was presented in paper II.5 of this meeting.

The main phenomena studied and reproduced during the former LMFBR experiments were:
- power to melt measurement,
- clad failure mechanisms (PCMI,...),
- fuel dispersion,
- fuel coolant interaction,
- transition phase.

The corresponding programs, carried on through international cooperations, induced an important know-how in different fields such as:

- development of models for calculations,
- fabrication of reliable test devices,
- accurate measurements in the test channel,
- post test fuel examinations.

Because of its design and taking into account this experience, CABRI has some major advantages which are transposable to RIA experiments in a Pressurized Water Loop.

III CABRI ADVANTAGES

The three main capabilities of the facility to reach the adequate test conditions and allowing accurate interpretation are:

3.1 Fast adjustable power transients

The power transients of the CABRI driver core are produced by a very fast depressurization of $^3$He, with which special rods (empty cladding tubes in the core) are previously filled up before the test.

By controlling this $^3$He depressurization through adjustable valves, it is possible to modulate the reactivity insertion, to produce different shapes of power transient and to control the energy release, within a 1 second duration limit, as shown on figures N° 1, 2 and 3.

3.2 In line fuel motion measurement

Fuel motion, because of its various effects, can have a crucial influence on the sequence and the consequences of an accident. This fuel motion (elongation, radial deformation and axial dispersion) is measured during the power transient by using a special device: the hodoscope (Figure 4).

The measuring problem of the hodoscope, i.e. monitoring the fuel mass density as a function of space and time, is solved as follows:

The fuel is measured by counting fast fission neutrons emerging from the test fuel since they are the most significant indicator for the presence of fissile material. They well penetrate the structures of the test loop, and, in the predominantly thermal neutron environment of the CABRI reactor, they can be discriminated by their energy from the fission-inducing neutron background.
The spatial resolution is provided by a so-called collimator, a neutron shield containing an array of channels viewing the test loop. By transmitting only fission neutrons from a small, well determined zone of the test section to a particular neutron detector, every channel defines one picture element of the test fuel distribution.

The resolution in time has to be high enough to follow fuel relocations. This implicates fast detectors and a fast acquisition system able to record high data rates (1ms minimum period).

3.3 **Accurate energy release measurement at rupture time**

The time and the location of the clad failure are determined by analyzing the "time of flight" of waves induced by the rupture:

- acoustic waves detected by two microphones,
- pressure waves detected by two pressure detectors,
- pressure waves, in case of flow ejection, detected by two flowmeters.

At that time the energy release is measured owing to:

- the power trace of the driver core measurement, using several power chambers,
- the coupling between the test pin and the driver core, obtained during an accurate heat balance on the test pin, previously to the test itself,
- the knowledge of the axial power profile into the test pin, given by the hodoscope.

Taking into account all these measurements and methods, it is commonly possible to determine the energy release in the test pin, at any time, within ±6% uncertainty.

See paper II.3 for more details.
IV POINTS PRESENTLY MISSING IN CABRI WITH REGARD TO PWR RIA

The following phenomena cannot be simulated and studied in CABRI by using the present facility with its sodium loop:

4.1. General aspects of non-representativitv of the sodium loop experiments

Performance of RIA tests in a sodium test channel limit a priori the representativity of the test and need correct understanding and interpretation in order to evaluate the applicability of the findings and conclusions.

The strong overcooling by sodium restrains strongly the temperature phase of representativity with regard to clad failure. Nevertheless, the important question of potential PCI failure could be addressed and the early phase of the RIA phenomenology, including clad straining, is covered by the sodium tests. The detailed sequence of events however, especially the post DNB and post-failure phenomena, cannot be studied. Also, the fission gas behaviour might be influenced significantly by the system pressure.

4.2 Effect of pressure

The release of fission gases depends on the internal pressure of the pin, and the possible consequences (ballooning and/or rupture by lack of ductility) are linked to the external pressure (and temperature) on the clad.

Furthermore the fission gases are the driving force for the fuel ejection after rupture which is then influenced by pressure.

So the temperature and pressure conditions influence significantly the representativity of the test.

4.3 Initiation and effect of DNB

The cladding temperature is obviously a key point during the RIA accidental sequence. So the heat transfer between water and clad is of importance, and particularly the Departure from Nuclear Boiling.

Indeed the DNB activation produces the elevation of the clad temperature and consequently the modification of its mechanical properties. As its temperature simultaneously increases the fuel is swelling and, if there is no early rupture by mechanical interaction, the heat rate being high, the ballooning of the clad could be reached, followed by rupture.

Of course this scenario cannot be simulated into a sodium loop and global tests in CABRI would give a validation of the model and of the cladding/water heat transfer correlations.

4.4 Post rupture phenomenology

The post rupture events such as fuel dispersion and fuel-coolant interaction are strongly influenced by the way how fuel and water come into contact. In particular experimental informations are lacking concerning the phenomena linked to the dispersion of highly divided solid fuel.

A pressurized water loop in CABRI would give experimental data to validate the codes and to appreciate the margins about the safety criteria.

So it is planned to implement a pressurized water loop with the objectives described in the following paragraph.
V OBJECTIVES

RIA experiments in a CABRI pressurized water loop would have multiple aims:

- to verify the principal conclusions deduced from the tests in sodium,
- to determine the clad rupture level in post DNB situation,
- to evaluate the fuel dispersion level,
- to evaluate the margins with regard to these levels,
- to validate the data base concerning the correlations and the materials properties,
- to establish a validation base for the calculation codes.

The following parameters would be studied:

- type of fuel (UO$_2$, MOX),
- amount of energy release,
- ramp rate of energy release,
- burn up (30 to 70 GWj/tM),
- physico-chemical structure of the clad.

Those global tests, associated to the analytic ones, and the validation of the calculation codes, would give elements to define new safety and dimensionning criteria.

VI CABRI TRANSFORMATION

The feasibility has been studied to modify the CABRI facility in order to be able to perform RIA experiments alternatively in pressurised water and in sodium. As those two elements are non compatible, the modification would be made according to the following principle.

6.1 Description of the existing facility

Shortly speaking CABRI is a water pool reactor, the core central part of which is fitted out with a test cell (see figures 6). An instrumented test device, containing the experimental pin, is introduced into the cell and the sodium loop is fitted to produce the required thermohydraulic conditions in the test channel (see figure 5). The methodology of the tests themselves is described in the paper II.3.

The present test cell is not disconnectable.

6.2 Replacement of the existing sodium cell

It is planed to dismantle the sodium cell and to replace it by a similar one but disconnectable using screwed flanged pipes.

After the removal of the sodium cell, the test site of the CABRI core will be free of sodium and a new pressurized water cell can be fitted.
6.3 **The pressurized water loop in CABRI**

A water loop has been studied in order to produce in a test channel the thermohydraulic conditions similar to the reactor case (350°C, 150 bar, 4m/s). This new loop is implemented in the facility (see figures 7 and 8) and connected to a water cell placed into the driver core, using screwed flanges too.

This pressurized water cell, dimensioned to the over pressure peak due to fuel/water interaction, receives a test section containing the test pin and instrumentation nearly equivalent to the sodium one.

The fuel motion measurement by the hodoscope is still operationnal with this new equipment.

The two test cells, when they are removed, are handled with a specific manipulator and stocked in confined silos.

At this opportunity, it is planned to remodel partly the instrumentation and command of the facility.

**VII. CONCLUSION**

The simulation capabilities of the presently available experimental facilities for RIA tests with high burn-up fuel are insufficient.

The pressurized water environment together with representative flow and temperature conditions is needed, especially for the study of post-DNB and post-failure behaviour of the highly irradiated LWR fuel.

A detailed study has demonstrated the feasibility of the implantation of a pressurised water loop into CABRI: the safe operation of the experimental facility and the achievement of the technical requirements will be possible.

The transformation of the presently available facility with its sodium loop into a double purpose facility allowing to operate alternatively the sodium loop and the PWR loop would need roughly one year. The facility could be operational by the mid of 1998 if the international nuclear safety community would confirm and support the IPSN evaluation.
FIG. 1 Fast power transient
FIG. 2 Structured power transient
FIG. 3 Medium power transient
FIG. 4  The fast neutron hodoscope in the CABRI facility
FIG. 5 Sodium test channel
FIG. 6 Sodium cell
FIG. 7 Pressurized water configuration
Session V: SUMMARY, ISSUES AND CONCLUSIONS

Co-Chair: F. SCHMITZ and M. RECIO

Closing remarks by Dr. Michel LIVOLANT
Deputy Director, IPSN, France
Member of CSNI Bureau ................................................................. 569

Summary and Conclusions .............................................................. 571

Recommendations to CSNI/PWG2 ................................................... 573

Programme .................................................................................. 575

List of Participants, Sorted in Alphabetical Order ............................... 581

List of Participants, Sorted by Country ............................................. 585
CLOSING REMARKS (M. LIVOLANT, Deputy Director of IPSN)

After three days of presentation and discussions by the best teams in the world in this field, after the active discussions between the specialists, and after the session conclusions presented just before, I have not the hope, neither in fact the ability, to give you any super conclusions.

I prefer to concentrate my short talk on some views concerning the future work, and how practically things could go on.

The basic question is: up to what burn-up the utilities would like to go. As an example, in France, the actual limit for assembly average burn-up is 47000 MWd/tu., with a demand from EDF to go up to 52000 MWd/tu., which is under instruction now. For that level, some data exists and there is a good chance to arrive to some conclusion with some analytical work - but there is a tendency to go up to 60000 MWd/tu. I understand from the summary of session 1 that some maximum peak pellet average burn-up of about 80000 MWd/tu. is already considered in some countries. There is no data at all at that burn-up level. For such levels, clearly, a further demonstration has to be made.

In the future of the subject, an important role will be played by calculations based on models as qualified as possible, for which it will be interesting to organize benchmarks in the CSNI groups.

It will be certainly interesting also to make in laboratories mechanical tests on irradiated cladding, or other analytical tests on mechanical or thermal hydraulics effects. It would be very useful that the corresponding data base could be open to international exchange, naturally in the respect of industrial confidentiality.

As owner of the Cabri facility, I am very interested to have a good appreciation of the need to make more in pile tests - and eventually in more representative conditions. Those tests are very expensive, and the modifications for installing a water loop for example or to enhance the core properties, represent a large amount of money.

However, if there is a real need expressed by the international community, we could make our best efforts to go in that direction.

CSNI has an important role to play in the domain, by stimulating work development, calculation benchmarks and giving advice on the need for experimental work, out of pile and in pile.

This could be the work of the ad hoc group of PWG2 in charge of this problem in the next future, may be with some extension of the ad hoc group to representatives of utilities on fuel matters.

Such orientation could be introduced and discussed at the next CSNI meeting.

The meeting was really a very successful one and I want to express the thanks of everybody to the people in OECD and Cadarache who made the hard work of organizing everything with so much efficiency. To be short, I will just mention the members of the Program Committee, Mr. Javier REIG and F. SCHMITZ for the technical organization and Mrs. MAGONI for the practical organization.

Thanks to them and the others.

Thanks to everybody for the quality of presentation and discussion.

I declare the closure of the meeting.

Michel LIVOLANT
Directeur Adjoint de l'IPSN
SUMMARY AND CONCLUSIONS OF THE CSNI SPECIALIST MEETING ON TRANSIENT BEHAVIOUR OF HIGH BURNUP FUEL

The CSNI Specialist Meeting on Transient Behaviour of High Burnup Fuel was held in Cadarache, France, from September 12th to 14th, 1995. It was hosted by the CEA Institut de Protection et de Sûreté Nucléaire (IPSN) at the Château located at the nuclear research centre of Cadarache. More than 125 experts from 15 OECD countries as well as experts from Russia and the IAEA attended the meeting. Thirty-two papers were presented in four sessions.

The purpose of the meeting was to bring together experts involved in the different activities related to high burnup fuel behaviour under transient conditions, and in particular during reactivity initiated accidents (RIA). The experts came from all involved parties, including research organisations, regulatory authorities, fuel designers and utilities. Information was openly shared and discussed on the integral experiments results, separate-effect tests findings and analytical assessments performed. Regulatory background and licensing implications were also included to provide the proper frame for the technical discussions.

The meeting focused on reactivity initiated accidents (RIA) because of the current interest in that subject and the significant amount of new technical information being generated. The meeting was structured around three main technical areas: integral experiments, separate effect tests and plant calculations, plus a background on the current regulatory status. Each of these areas corresponded to a separate session. The general conclusions from the meeting as well as the recommendations from the ad-hoc group to CSNI/PWG 2 are included in the following points.

GENERAL CONCLUSIONS

1. Three major test programs (SPERT, NSRR and CABRI) with high burnup fuel have each found a specimen that failed at very low energy deposition.

2. These low energy failures are all believed to be caused by pellet-clad mechanical interaction, assisted by embrittlement of the Zircaloy cladding at regions with high local concentrations of hydrides.

3. Test conditions were not matching, in some parametric details, with the expected reactor conditions and the effect of these differences are not yet fully understood. Despite these aspects of non-direct-representativity, important insight is gathered from the tests on loading and resistance factors and burnup effects are revealed.

4. Tests with different cladding (Zr-1Nb) and large power pulse width conditions (~700ms) produced ductile failures at high energies in contrast to brittle failures seen in Zircaloy clad fuel rods subjected to short pulses (≤ 10ms).
5. Plant calculations are consistently showing pulse widths of 25 to 75ms and indicate that rod worth and the loading pattern are important parameters. 3D calculations reveal significant conservatism of present licensing methods using 2D/1D calculations.

6. Changes in material properties (e.g. thermal conductivity) will affect neutronic calculations. Uncertainties have to be considered in future evaluations.

7. Important materials properties at high burnup have already been measured and mechanical properties testing is just being initiated for conditions of RIA.

8. Fission gas expansion leads to grain separation and fuel fragmentation in the tests and might affect significantly the accident consequences if, after failure, finely fragmented fuel particles are ejected into the coolant water and produce violent boiling and pressure generation from fuel-coolant interactions.

9. The significance of the RIM effect is presently not fully understood or demonstrated. Its contribution to the failure phenomena is questioned. For larger pulse widths, calculations show that the thermal level reached in the RIM region is significantly decreased and associated fission gas phenomena might be mitigated.

10. Presently, there is no test facility able to perform RIA experiments under fully representative plant conditions of:

- coolant nature,
- system pressure,
- flow and temperature conditions,
- controlled pulse width,
- sufficiently high energy deposition (safety margin with regard to plant calculations).

The CEA/IPSN introduced its project to implement a pressurised water loop into the CABRI reactor to resolve this deficiency.
RECOMMENDATIONS TO CSNI/PWG 2

From the Specialist Meeting and its conclusions, the ad-hoc group recognises that:

- One of the important achievements of the CSNI meeting was to provide for the first time a forum for an extensive exchange of technical information.
- Common technical positions exist for some of the phenomena involved, but there are still technical issues under discussion.
- Several countries have on-going programs and/or are planning activities to improve the understanding of high burnup fuel behaviour under transient conditions.

Therefore the ad-hoc group proposes to CSNI/PWG 2:

1. To meet at least once more (suggested timing April 1996) to prepare a technical position paper that will:
   - Consider the different options to continue providing a proper forum for exchange of technical information.
   - Discuss the needs and rationale for any further work to better understand the transient behaviour of high burnup fuel.
   - Consider the different options to co-ordinate and efficiently integrate OECD member countries activities on this topic.

2. To enlarge the ad-hoc group with additional industry representatives and a Russian expert to properly take into account the various activities and technical points of view. This extension should be limited to those institutions currently performing a significant work in this area in to keep the size of the group reasonably small.

3. To endorse the publication of the Proceedings of the Specialist Meeting as a general distribution document. This document consists of the papers presented, the main facts from the discussion that took place and the summary and conclusions prepared by the session chairmen and the Programme Committee.
CSNI Specialist Meeting on Transient Behaviour of High Burnup Fuel

Cadarache, France, 12th-14th September 1995

PROGRAMME
Tuesday, 12th September, 1995

General Meeting Chairman: Dr. Franz SCHMITZ (CEA/IPSN, France)

Opening Session

08:30 Registration

09:30 Opening of the Meeting

Welcome - Dr. Michel SUSCILLON (CEA)
Director of the Cadarache Research Center

Opening Remarks - Mr. Gianni FRESCURA (OECD/NEA)
Head, Nuclear Safety Division

Introductory Remarks - Dr. Alain TATTEGRAIN (IPSN)
Deputy Head Department of Safety Research

Session I: Regulatory Background

Co-Chair: R. MEYER (NRC, USA) and C. MAEDER (HSK, Switzerland)

10:00 « USNRC Review of High Burnup Fuel Regulatory Requirements »
L.E. PHILLIPS

10:20 « French Safety Authority Position »
D. LAGARDE, V. JACQ, J. LEWI, M. CHAMP

10:50 « Regulatory Status on Transient Behaviour of High Burnup Fuel and Related Research Activities in Japan »
T. SATO, T. FUJISIRO

11:10 « German Licensing Approach and Consequences for High Burnup Fuel »
S. LANGENBUCH, Ch. FABER

11:30 COFFEE BREAK
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<th>Time</th>
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<tr>
<td>12:00</td>
<td>« Behaviour of High Burnup PWR Fuel Under a Simulated RIA Condition in the NSRR »</td>
<td>T. FUKETA, Y. MORI, H. SASAJIMA, K. ISHIJIMA, T. FUJISHIRO</td>
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<td>14:00</td>
<td>« The Experimental Test Programme for the Study of High-Burnup PWR Rods Under RIA Conditions in the CABRI Core »</td>
<td>M.C. ANSELMET-VITIELLO, F. ARREGHINI, M. HAESSLER</td>
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<td>14:25</td>
<td>« Cladding and Fuel Modifications of a 60 GWj/tM Irradiated Rod During a Power Transient Performed in the CABRI Reactor »</td>
<td>P. MENUT, D. LESPIAUX, M. TROTABAS</td>
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<td>14:50</td>
<td>« The Behaviour of Irradiated Fuel Under RIA Transients: Interpretation of the CABRI Experiments »</td>
<td>J. PAPIN, H. RIGAT, J.P. BRETON</td>
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<td>15:15</td>
<td>« Development and Realization of Research Programme for High Burnup Fuel Rods Behaviour Analysis under RIA Condition at IGR Pulse Reactor »</td>
<td>V. ASMOLOV, L. YEGOROVA</td>
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<td>COFFEE BREAK</td>
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<td>16:10</td>
<td>« Primary Factors Causing the Failure of High-Burnup LWR Fuel Rods During Simulated Reactivity Initiated Accidents »</td>
<td>R.K. McCARDELL, R.O. MEYER</td>
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<td>16:35</td>
<td>« Unexpected Transient on an Experimental Instrumented Fuel Rod in BR2 »</td>
<td>S. BODARD, B. COUPE, V. SOBOLEV, M. LIPPENS</td>
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<td>17:00</td>
<td>« Studsvik's Experience Related to LWR Fuel Behaviour at High Burnup »</td>
<td>H. MOGARD, M. GROUNES</td>
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Wednesday, 13th September 1995

Session III: Plant Calculations

Co-Chair: S. LANGENBUCH (GRS, Germany)
and K. VALTONEN (STUK, Finland)

08:30  « A Best Estimate Assessment of Rod Ejection Fuel Duty in PWRs »
B.J. JOHANSEN, M.J. HONE, D.H. RISHER, S. RAY, R.A. HOLLAND,
U. BACHRACH, G.E. DERYLO

08:55  « Study of the Rod Ejection Transient on a PWR Related to High Burnup
Fuel Rupture Risk »
S. STELLETTA

09:20  « On the Role of Burnup Effects of Fuel Properties in RIA Analysis »
R. KYRKI-RAJAMAKI

09:45  « Analysis of the Fuel Behaviour under Rod Ejection Accident in the PWR »
C.B. LEE, B.O. CHO, O.H. KIM, Y.B. KIM, K.S. SEO, J.G. CHUNG,
C.C. LEE

10:10 « Realistic Scoping Study of Reactivity Insertion Accidents for a Typical
PWR and BWR Core »
A.F. DIAS, L.D. EISENHART, L.J. AGEE, L.D. EISENHART

10:35  COFFEE BREAK

11:55 « Methodology and Results of RIA Studies at Siemens »
H. FINNEMANN, D. BENDER, R. EBERLE, H. BAUER, F. WEHLE

11:20 « Analyzing the BWR Rod Drop Accident in High-Burnup Cores »
D.J. DIAMOND, L. NEYMOTIN, P. KOHUT

11:45 « Investigations Related to Increased Safety Requirements for Reactivity
Initiated Accidents »
F. HOLZGREWE, J.M. KALLFELZ, M.A. ZIMMERMANN, C. MAEDER,
U. SCHMACKER

12:10 « Analyses of Rod Drop Accidents Using a Three-Dimensional Transient
Code for Reactivity-Initiated Events of Boiling Water Reactors »
A. OMOTO, T. OTA, M. NAGANO, S. IZUTSU

12:35 « Realistic Evaluation of RIA in BWRs »
J.G.M. ANDERSEN, G.A. POTTS, J.F. KLAPPROTH, R. HARRINGTON,
C.L. HECK, R.A. RAND

13:00 LUNCH

14:00 - TECHNICAL VISIT TO CABRI REACTOR
17:30 (see annexed programme)
19:30 DINNER HOSTED BY CEA/IPSN AT CASSIS
Thursday, 14th September, 1995

Session IV: Separate-Effect Test and Analysis

Co-Chair: V. LANGMANN (Ontario Hydro, Canada) and A. DELBRASSINE (SCK/MOL, Belgium)

09:00 « High Burnup Phenomena-Results from Experiments in the Halden Reactor »
W. WIESENACK

09:20 « Tensile Properties of Irradiated Zircaloy-4 Cladding Submitted to Fast Transient Loading »
M. BALOURDET, C. BERNAUDAT

09:40 « Influence of Locally Concentrated Hybrids on Ductility of Zircaloy-4 »
F. NAGASE, K. ISHIJIMA, T. FURUTA

10:00 « A 2D-3D Finite-Element Approach of Fuel Rod Thermomechanical Behaviour During a RIA »
C. BERNAUDAT, J.P. BERTON, P. PERMEZEL

10:30 COFFEE BREAK

11:00 « Evaluation of RIA Experiments and their Impact on High Burnup Fuel Performance »
O. OZER, R. YANG, S. YAGNIK, L. AGEE, Y. RASHID, R. MONTGOMERY

11:20 « Impact of Fission Gas Behaviour on Irradiated Fuel Behaviour at Extended Burnup under RIA Conditions »
F. LEMOINE, F. SCHMITZ et al.

11:40 « Modelling of Phenomena Associated With High Burnup Fuel Behaviour During Overpower Transients »
H.E. SILLS, V.J. LANGMANN, F.C. IGLESIAS

12:00 « High-Burnup Modelling Changes to NRC Fuel Performance Codes that Impact Reactivity Initiated Accidents »
C.E. BEYER, D.D. LANNING, L.J. SIEFKEN

12:30 Invited Paper from Host Organisation

« The Project to Implement into CABRI a Pressurized Water Loop. Motivations and Objective of the Future Test Program »
J. FURLAN, M. HAESSLER, F. SCHMITZ, J. PAPIN, A. TATTEGRAIN

13:00 LUNCH
Session V: Summary, Issues and Conclusions

Co-Chair: F. SCHMITZ (IPSN, France) and M. RECIO (CSN, Spain)

14:00 Session I conclusions
R.O. MEYER, C. MAEDER

14:25 Session II conclusions
T. FUJISHIRO, M. HAESSLER

14:50 Session III conclusions
S. LANGENBUCH, K. VALTONEN

15:15 Session IV conclusions
V. LANGMANN, A. DELBRASSINE

15:40 Closing remarks

Dr. Michel LIVOLANT (IPSN, France)
Deputy Director
Member of CSNI Bureau
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