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THE PASSIVE RESPONSE OF THE INTEGRAL FAST REACTOR CONCEPT TO THE CHILLED INLET ACCIDENT*

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THE PASSIVE RESPONSE OF THE INTEGRAL FAST REACTOR CONCEPT TO THE CHILLED INLET ACCIDENT

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ABSTRACT

Simple methods are described for bounding the passive response of a metal fueled liquid-metal cooled reactor to the chilled inlet accident. Calculation of these bounds for a prototype of the Integral Fast Reactor concept shows that failure limits -- eutectic melting, sodium boiling and fuel pin failure -- are not exceeded.

INTRODUCTION

An innovative reactor concept known as the Integral Fast Reactor (IFR) addresses a number of the issues raised in the operating, licensing, and construction environments of the recent past. The concept envisions a nuclear reactor that has inherently safe characteristics and will never need fuel management on- or off-site during its lifetime, making it virtually impossible to divert fissionable material for weapons use. The key features of the IFR concept are metal fuel, liquid-metal coolant, a pool-type reactor, and integral reprocessing.

The reactor physics issues of designing for passive safety were reported recently.[1] Essentially one designs for five generic anticipated transient without scram events - loss of flow (LOF), loss of heat sink (LOHS), rod runout transient overpower (TOP), pump overspeed and chilled inlet temperature. Simple methods exist for analyzing the first four events. No simple methods, however, have been developed for the chilled inlet accident. The focus of this paper is on the development of such methods.

A chilled inlet accident is initiated when the heat removal rate from the primary system exceeds the normal reactor heat generation rate. Reactor inlet temperature in turn decreases until a new plant equilibrium state is reached, or the overcooling ceases. In this paper, the primary flow is assumed to remain at the normal full power value. Physical mechanisms for inducing primary system overcooling include steam generator blowdown, intermediate pump overspeed, loss of feedwater heating and excessive turbine load. As we shall see for a prototype of the IFR, it may not be necessary to list and analyze all chilled inlet initiators in order to ensure core safety.

Analysis of the unprotected chilling event is simplified by the fact that the only way this event can influence the core is through the reactor inlet temperature. The response of the core to a change in inlet temperature is given by the quasi-static reactivity balance

$$0 = \Delta\rho = (\overline{P} - 1)A + (\overline{P}/\overline{W} - 1)B + \delta T_{in}C + \Delta\rho_{ext} \tag{1}$$

where

$\overline{P},\overline{W}$ = normalized power and flow, respectively,
δT_{in} = change from normal coolant inlet temperature,
$\Delta\rho_{ext}$ = externally imposed reactivity,
A = net (power-flow) reactivity decrement (cents),
B = power/flow coefficient (cent/100% $\overline{P}/\overline{W}$), and
C = inlet temperature coefficient of reactivity (cent/°C).

This equation assumes that the cooling rate is slow enough to preclude nonequilibrium of both fuel pin temperatures and delayed neutron population. This is the case in pool type reactors. The above equation can be solved for the new power level after the core neutronics inherently adjusts to the changed inlet temperature.

This paper describes physical limits that bound the drop in core inlet temperature for chilled inlet accidents. The idea is that no detailed evaluation of accidents is required if the value of the temperature drop is limited by physical mechanisms and that the core is safe for this value. Two cases are treated. First, we analyze the steam generator blowdown event as it potentially can cause the severest overcooling of all chilled inlet initiators. A non-flow control mass analysis yields an upper bound for the reactor's passive response that is independent of break size area. Second, we show that freezing of the intermediate cold leg sodium physically limits the overcooling of the primary system and thereby bounds the core response for all chilled inlet initiators.

STEAM GENERATOR BLOWDOWN

A blowdown of the steam generator would occur if the water side pressure boundary were breached. Without automatic or operator intervention, the intermediate system sodium would overcool, which in turn would overcool the primary core inlet temperature. The magnitude of the temperature decrease is a function of the water inventory and the heat capacities of the primary and intermediate systems.

We describe a simple method for estimating the magnitude of the decrease without having to predict complex transient phenomenon. The method makes use of a non-flow control mass approach to avoid having to explicitly deal with the break size area. The estimate assumes that the available water inventory interacts in a way that removes the thermodynamic maximum amount of energy from the plant sodium and structure and that on completion, no further water is available for cooling. At that time the chilled inlet accident is transformed into a LOHS accident.

A control mass is defined for the plant shown in Figure 1 and consists of the sodium and structure in the primary and intermediate systems and the water and structure on the water side. The fuel is not included. It is assumed that all the blowdown mixture enters the containment. An envelope is envisioned that contains this control mass and grows in size so as to always contain the escaping blowdown mixture. There is no mass flux across this envelope. Except for the influx of heat from the fuel, this envelope is adiabatic.

The first step is to determine the thermodynamic maximum possible change in control mass internal energy resulting from the blowdown. To do so, the sodium, structure and water components of the control mass are brought into thermal equilibrium while preserving their total internal energy. With the components in thermal contact, the water is expanded reversibly to a final pressure of one atmosphere. The flow of heat from the fuel into the control mass is, for the moment assumed zero. The change in internal energy of the control mass is

$$\Delta U_{\mathrm{rev}} = - W_{\mathrm{rev}} \tag{2}$$

where W_{rev} is the work performed by the control mass as it expands.

The second step is to determine the change in control mass internal energy for irreversible expansion. Specifically, the water no longer expands reversibly during expansion. However, the sodium and structures are assumed to have zero thermal resistance, a requirement removed in the final step. The internal energy change is

$$\Delta U_{\mathrm{irr}} = - W_{\mathrm{irr}} \tag{3}$$

where W_{irr} is the work performed by the control mass as it expands. The change in the average temperature of the control mass is represented by ΔT_{irr}. It is convenient to define an *irreversibility factor*

$$\eta = \frac{W_{irr}}{W_{rev}} \tag{4}$$

so that

$$\Delta U_{irr} = \eta \, \Delta U_{rev}. \tag{5}$$

The third step is to consider the effect of the heat addition from the fuel. The first law of thermodynamics implies the following equality

$$\Delta U_{\mathrm{irr\text{-}Q}} = \Delta U_{\mathrm{irr}} + (Q - \delta W) \tag{6}$$

where

Q - heat flow from the fuel into the control mass, and
δW - the increase in work performed as a result of heat addition Q.

The second law implies that the additional work obtained as a result of adding heat must be less than the amount of heat added,

$$Q - \delta W \geq 0. \tag{7}$$

Combining the above equation with Eqs. (5) and (6),

$$\Delta U_{irr-Q} \geq \Delta U_{irr} - \eta \, \Delta U_{rev}. \tag{8}$$

The change in control mass average temperature is represented by ΔT_{irr-Q}. Eq. (8) implies,

$$\Delta T_{irr-Q} > \Delta T_{irr}.$$

The final step is to remove the idealization of zero thermal resistance between the sodium and structures. Finite thermal resistances give rise to the thermal gradient across the plant during normal operation. This gradient increases during blowdown. Clearly, the amount of heat flowing from the primary system into the intermediate system and from the intermediate system into the water system between the start of blowdown and when the water reaches one atmosphere pressure has to be less than in the third step. Thus the actual internal energy changes are related to those in that step by

$$\Delta U_{ACTUAL-P} > \Delta U_{irr-Q-P} \tag{9}$$

$$\Delta U_{ACTUAL-I} > \Delta U_{irr-Q-I}. \tag{10}$$

where the quantities on the left are primary and intermediate system averages. Hence,

$$\Delta T_{ACTUAL-P} > \Delta T_{ACTUAL-I} > \Delta T_{irr-Q}. \tag{11}$$

Typically the hot pool inventory is small compared to the cold pool so that

$$\Delta T_{ACTUAL-P} - \Delta T_{\substack{cold\\pool}}$$

This leads to the bound we seek for the core inlet temperature,

$$\Delta T_{\substack{cold \\ pool}} > \Delta T_{irr-Q} > \Delta T_{irr} \ . \tag{12}$$

Specific models are available[2] for computing, as part of the general approach described above, the initial state of thermal equilibrium, the final state at the end of isentropic expansion and the final state at the end of irreversible expansion.

The quasi-static core power corresponding to the cold pool temperature is obtained from Eq. (1). It is calculated for a specific plant design later in this paper and the result is given in Table II.

INTERMEDIATE COLD LEG FREEZING: THE LIMITING EVENT

We would like to identify the worst conditions that can occur in the core over the spectrum of chilled inlet initiators. The approach we use makes it unnecessary to sort through all initiators. Rather, we use the fact that the drop in cold pool temperature is physically limited by freezing of the intermediate system sodium.

To see this, consider that a) the evolution of the state of the primary system is determined exclusively by the sodium flowrate and temperature in the cold leg of the intermediate system, b) the maximum heat removal rate from the primary system at any time occurs for maximum flowrate and minimum temperature (sodium solidus) in the intermediate cold leg and c) the feedback coefficients of the core and plant time constants are such that the cold pool temperature equilibriates in an overdamped manner to changes in cold leg conditions. It follows that the minimum cold pool temperature possible occurs for the primary system in equilibrium with full flow and sodium freezing conditions in the cold leg. Since the core power increases with decreasing inlet temperature for typical core parameter values, the maximum core power occurs at this minimum cold pool temperature.

We calculate the cold pool temperature at this equilibrium condition. The outlet of the primary side of the intermediate heat exchanger is related to the intermediate flowrate and sodium solidus temperature through

$$T_{PC} = K_1 \, T_{PH} + K_2 \, T_S \tag{13}$$

where

T_{PC} — primary system cold pool temperature,
T_{PH} — primary system hot pool temperature,
T_S — intermediate system cold leg temperature set to sodium solidus.

The coefficients in the above equation are given by

$$K_1 = \frac{1-R}{K-R} \tag{14}$$

and

$$K_2 = \frac{K-1}{K-R} \quad . \tag{15}$$

In turn the variables in these two equations are given by

$$K = \exp\left\{UA\left[\frac{1}{(WC_p)_P} - \frac{1}{(WC_p)_I}\right]\right\} \tag{16}$$

and

$$R = \frac{(WC_p)_P}{(WC_p)_I} \tag{17}$$

where

UA = overall heat transfer coefficient,
W = flowrate,
C_p = specific heat,
P = subscript denoting primary system, and
I = subscript denoting intermediate system.

Separately, the core power at full flow conditions is from Eq. (1)

$$P = 1 - \frac{C\delta T_i}{A + B} \quad . \tag{18}$$

Eqs. (13) and (18) can be combined to express the inlet temperature at full flow as

$$\delta T_i = \frac{\Delta T_0 - \left(\frac{1-K_1}{K_1}\right)T_{PC_0} + \frac{K_2}{K_1}T_S}{\frac{C\Delta T_0}{A + B} + \frac{1 - K_1}{K_1}} \tag{19}$$

where

ΔT = reactor temperature rise, and
0 = subscript denoting initial full power steady state conditions.

APPLICATION

The passive response of an IFR prototype to unprotected chilling accidents was analyzed using the foregoing methods. Plant design parameters are summarized in Table I. The core conditions predicted for steam generator blowdown and for intermediate sodium freezing were compared against three safety limits -- eutectic temperature, sodium boiling and overpower at pin failure in overpower experiments. The first two limits were converted to normalized overpower through the expressions

$$\overline{P}_{SL,j} = \frac{T_j - (T_{i_0} + \delta T_i)}{\Delta T_0 \ f} \tag{20}$$

and

$$\overline{P}_{SL,j} = 1 - \frac{C\delta T_i}{A + B}$$

where

$\overline{P}_{SL,j}$ = normalized overpower for safety limit j ,
T_j = temperature for safety limit j,
T_{i_0} = reactor inlet temperature at full power steady state conditions,
f = peaking factor, ratio of maximum subchannel coolant temperature rise over all life to reactor temperature rise.

The main result of the analysis is that safety limits are not exceeded. The bounds for normalized overpower calculated using the steam generator blowdown method and the sodium freezing method are shown in Table II. The peaking factor was calculated from conditions at normal operation.

It is evident that the overpower predicted for blowdown of the entire water side (steam generator, feedwater and condensate) exceeds the overpower at which sodium freezing occurs. Based on these results, the overpower during blowdown would be limited by freezing of the sodium in the intermediate system. If, however, the blowdown inventory is only that of the steam generator, as would be the case if the automatic shutoff of the feedwater pumps tripped the pumps, the overpower is limited to 1.01.

SUMMARY

Physical limits that bound the temperature and power response of a metal fueled liquid-metal reactor to the unprotected chilled inlet accident were developed. No detailed evaluation of initiators is required if these bounds when calculated lie below core safety limits. Two cases were treated: the steam generator blowdown event, which potentially can cause the most severe core conditions, and freezing of the intermediate system cold leg during quasi-static overcooling, which bounds core conditions for all chilled inlet initiators. Calculation of these bounds for a prototype of the Integral Fast Reactor concept showed that failure limits -- eutectic melting, sodium boiling and fuel pin failure -- are not exceeded.

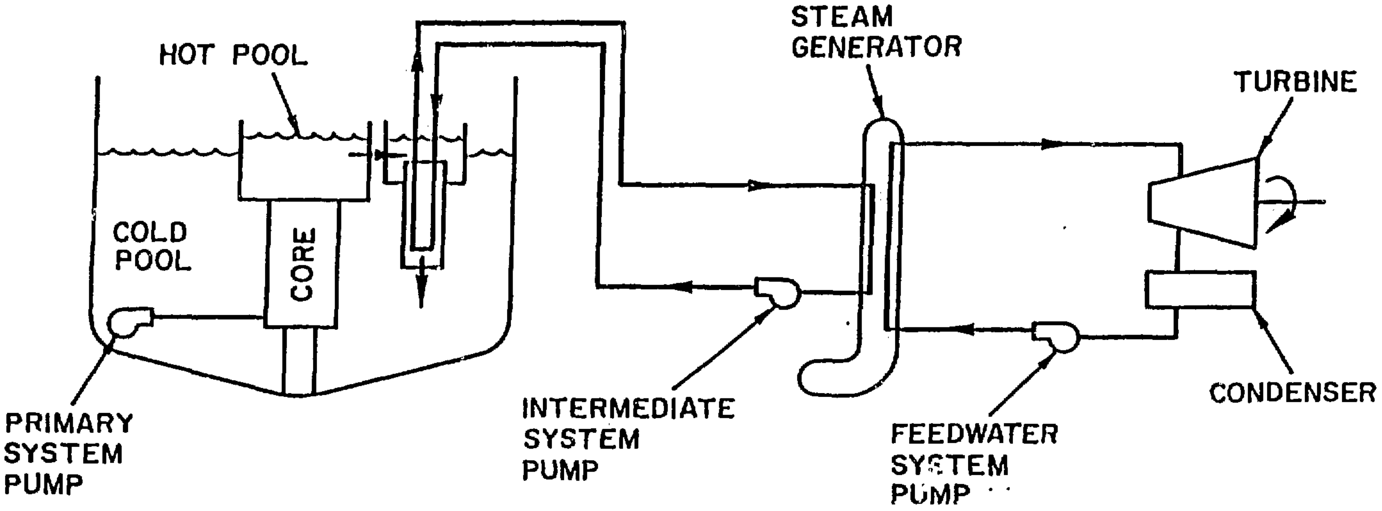

Fig. 1. Liquid-Metal Pool Type Reactor

TABLE I. Parameter Values for Representative Pool Plant

$M_{NA\text{-}P,}$, mass of primary sodium,	kg	800,000
$M_{NA\text{-}I,}$, mass of intermediate sodium,	kg	47,000
$M_{SG,}$, mass of steam generator water,	kg	2,300
$M_{FW,}$ mass of feedwater,	kg	35,000
$M_{C,}$ mass of condenser water,	kg	140,000
$M_{S\text{-}P,}$ mass of primary structure,	kg	390,000
$M_{S\text{-}I,}$ mass of intermediate structure,	kg	27,000
$M_{S\text{-}SG,}$ mass of steam generator structure,	kg	280,000
A + B,	cents	-50
C,	cents/°C	-0.33
ΔT_0,	°C	150
T_{i0}	°C	357
$(wC_p)_I$,	Joules/S	5.788×10^6
$(wC_p)_p$,	Joules/S	5.890×10^6

TABLE II. Comparison of Overpower Bounds and Core Safety Limits for Chilled Inlet Accident

Overpower bound, $\overline{P}$	
Blowdown of entire water side	2.28
Blowdown of steam generator inventory only	1.01
Freezing of intermediate system cold leg	2.25
Safety limits expressed as overpower, $\overline{P}_{SL,j}$	
Eutectic	2.58
Sodium boiling	4.36
Pin failure in overpower experiments	4.0

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