For presentation at the Topical Meeting on Safety of Next Generation Power Reactors, April 1-5, 1988, in Seattle, WA

Radial Core Expansion Reactivity Feedback in Advanced LMRs: Uncertainties and Their Effects on Inherent Safety*

by

R. A. Wigeland and T. J. Moran

Argonne National Laboratory
Reactor Analysis and Safety Division
Applied Physics Division
9700 S. Cass Avenue
Argonne, IL 60439

CONF-8804155--1

DE88 011944

*Work supported by the U.S. Department of Energy, Office of Technology Support Programs under Contract W-31-109-Eng-38.

For presentation at the Topical Meeting on Safety of Next
Generation Power Reactors, April 1-5, 1988, in Seattle, WA

Radial Core Expansion Reactivity Feedback in Advanced LMRs: Uncertainties and Their Effects on Inherent Safety

by

R. A. Wigeland and T. J. Moran

Argonne National Laboratory
Reactor Analysis and Safety Division
Applied Physics Division
9700 S. Cass Avenue
Argonne, IL 60439

Abstract

An analytical model for calculating radial core expansion, based on the thermal and elastic bowing of a single subassembly at the core periphery, is used to quantify the effect of uncertainties on this reactivity feedback mechanism. This model has been verified and validated with experimental and numerical results. The impact of these uncertainties on the safety margins in unprotected transients is investigated with SASSYS/SAS4A, which includes this model for calculating the reactivity feedback from radial core expansion. The magnitudes of these uncertainties are not sufficient to preclude the use of radial core expansion reactivity feedback in transient analysis.

Radial Core Expansion Reactivity Feedback in Advanced LMRs: Uncertainties and Their Effects on Inherent Safety

by

R. A. Wigeland and T. J. Moran

Argonne National Laboratory
Reactor Analysis and Safety Division
Applied Physics Division
9700 S. Cass Avenue
Argonne, IL 60439

The reactivity feedback from radial expansion of the reactor core is usually the dominant negative reactivity feedback available for mitigating the consequences of unprotected accidents in advanced LMRs. The outcome of unprotected loss-of-flow, transient overpower, and loss-of-heat-sink transients is largely controlled by the timing and magnitude of this reactivity feedback mechanism. Unfortunately, this component of the overall reactivity feedback has also been associated with the greatest amount of uncertainty, especially where subassembly "bowing" is concerned. As a result, the contributions from this component during a transient would be either ignored, or arbitrarily reduced, with subsequent detrimental effects on the predicted reactor response to these unprotected accidents. In an attempt to improve this situation, a mechanistic model of radial core expansion based on the thermal and elastic bowing of a single driver assembly at the core periphery was included in the plant transient analysis code, SASSYS/-SAS4A.[1,2,3] In this paper, estimates of the modeling uncertainties were obtained with an algebraic version of this model of the radial core expansion, which has been verified and validated by comparison with a detailed core deformation code, NUBOW-3D, and with experimental data from the FFTF reactor. The resulting effect of these uncertainties on the transient reactor response was evaluated using SASSYS/SAS4A, providing an indication of their importance to the inherent safety of the plant. The result of this analysis and parametric investigation is that calculations of radial core expansion reactivity feedback can be performed with a greater degree of confidence, and the predicted reactor response can take advantage of the availability of this important reactivity feedback mechanism.

Analytical Model for Radial Core Expansion

The single assembly analytical model for the evaluation of radial core restraint designs [4] and the uncertainties associated with radial core expansion reactivity feedback has been developed for a fast reactor using a limited-free-bow core restraint design. Limited-free-bow core restraint has been shown to be preferred for radial expansion [5]. This model is restricted to those bowing regimes where the plane of above-core load pads (ACLP) is compacted to the point where the outermost driver assemblies are restrained at the ACLP from further compaction by a continuous network of contacting load pads, and where the top load pads (TLP) of the outer driver assemblies are restrained from further radial expansion by continuous load paths to the TLP restraint ring. Elementary beam theory is used to calculate the elastic bow of a average driver subassembly at the core periphery subject to temperature dependent boundary conditions at the nozzle support, ACLP, and TLP and subject to thermal and inelastic bowing deformations. The following design parameters are considered: grid plate temperature, core temperature rise, restraint ring temperature, grid plate and restraint ring thermal expansion coefficients, duct material properties (thermal expansion, swelling and creep), nozzle support conditions, core radius, core axial location, core height, driver assembly radial thermal gradient, ACLP location and compressibility, and gaps at the ACLP and TLP elevations.

Fig. 1 defines dimensionless versions of these parameters schematically. Physical lengths are all scaled by the distance, L, from the nozzle support to the TLP. Dimensionless temperatures are formed by multiplying a temperature by the corresponding material thermal expansion coefficient, e.g., the grid plate temperature times the grid plate thermal expansion coefficient is the dimensionless number τ_1. μ_2 is the ratio of the duct thermal expansion coefficient to the grid plate thermal expansion coefficient. μ_3 is that same ratio for the restraint ring. The parameter λ is a dimensionless compressibility of the ACLP plane:

$$\lambda = EI/KL^3$$

were EI is the assembly bending stiffness and K is the effective stiffness of the ACLP plane.

The thermal bow shape is represented by the function $\xi(\chi)$:

$$\xi(\chi) = 0 \qquad 0 \le \chi \le \beta-\chi$$

$$\xi(\chi) = -\left[\frac{(\chi+\alpha-\beta)^3}{12\alpha}\right]\tau_r \qquad \beta-\alpha \le \chi \le \beta+\alpha$$

$$\xi(\chi) = -\left[\frac{(\chi-\beta)^2}{2} + \frac{\alpha^2}{6}\right]\tau_r \qquad \beta+\alpha \le \chi \le 1$$

where τ_r is the ratio of the dimensionless transverse temperature gradient to the dimensionless core temperature rise, τ_2. The dimensionless, thermal-bow, radial displacement is given by $\xi(\chi)\,\tau_2$. Radial displacement due to inelastic bowing caused by irradiation enhanced creep and swelling is independent of temperature changes during a transient. It is represented by $\eta(\chi)$.

If the core is not restrained, the free bow of an assembly is given by

$$\psi(\chi) = \rho_o(1+\tau_1) + \xi(\chi)\tau_2 + \eta(\chi)$$

which we represent in the form

$$\psi(\chi) = \sum_{i=0}^{3} \psi_i(\chi)\tau_i$$

where $\psi_0(\chi) = \rho_o + \eta(\chi)$

$\psi_1(\chi) = \rho_o$

$\psi_2(\chi) = \xi(\chi)$

$\psi_3(\chi) = 0$

and $\tau_0 = 1$

For the restrained core this free bow has a net interference at the TLP of

$$\zeta(1) = \sum_{i=0}^{3} \zeta_i(1)\tau_i$$

where

$$\zeta_0(1) = \varepsilon(1) - \eta(1),$$
$$\zeta_1(1) = \mu_3\varepsilon(1) - (1-\mu_3)\rho_0,$$
$$\zeta_2(1) = -\xi(1), \text{ and}$$
$$\zeta_3(1) = \rho_0 + \varepsilon(1)$$

The net interference at the ACLP is

$$\zeta(\gamma) = \sum_{i=0}^{\tau} \zeta_i(\gamma)\tau_i$$

where

$$\zeta_0(\gamma) = \varepsilon(\gamma) - \eta(\gamma),$$
$$\zeta_1(\gamma) = \mu_2\varepsilon(\gamma) - (1-\mu_2)\rho_0,$$
$$\zeta_2(\gamma) = \rho_0 + \varepsilon(\gamma) - \xi(\gamma), \text{ and}$$
$$\zeta_3(\gamma) = 0.$$

The elastic bow shape is obtained by solving the Bernoulli-Euler beam equation for these two boundary conditions and a pinned boundary condition at the nozzle support, χ=0. The solution [4] can be expressed in the form

$$\rho(\beta) = \sum_{i=0}^{3} C_i\tau_i, \tag{1}$$

where C_i are the temperature coefficients of the radial expansion at the core midplane.

$$C_i = \frac{\beta}{\gamma}\zeta_i(\gamma) + \beta(\phi-1)\left[\frac{\zeta_i(\gamma)}{\gamma} - \zeta_i(1)\right] + \psi_i(\beta) \tag{2}$$

The first term, $\frac{\beta}{\gamma}\zeta(\gamma)$, represents a rotation of the assembly about the nozzle pinned support sufficient to account for the net interference at the ACLP.

The term $\left[\frac{\zeta(\gamma)}{\gamma} - \zeta(1)\right]$ is the net interference at x = 1 after this rotation.

The term $\beta(\phi-1)$ is the elastic displacement at x=β due to a net interference at x = 1. It might be termed a bowing influence coefficient and depends only on the geometric terms β and γ and the elastic parameter λ.

$$\phi = \frac{\frac{2-\gamma}{1-\gamma} - \frac{\beta^2}{\gamma(1-\gamma)}}{2 + \frac{6\lambda}{\gamma^2(1-\gamma)^2}}$$

The physical radius is ρL, where L is the distance from the nozzle support to the TLP. Changes in reactivity, R, are given by

$$\Delta R = WL\Delta\rho = WL(C_1\Delta\tau_1 + C_2\Delta\tau_2 + C_3\Delta\tau_3) \quad (3)$$

where W is the uniform dilation reactivity worth. In this manner the model predicts reactivity changes as a function of changes in the grid plate temperature, the core temperature rise and the restraint ring temperature. During the early part of a transient, ΔR is controlled by τ_2. The corresponding dilation coefficient, C_2, contains terms associated with both load pad expansion and duct bowing, with bowing contributing approximately 30-50%, depending on the details of the core restraint design. Later in the transient $\Delta\tau_1$ and $\Delta\tau_3$ may become important. C_1 is of the same magnitude as C_2 for most designs, but the ring coefficient is of opposite sign and small.

Validation

The model available in SASSYS/SAS4A has recently been validated with FFTF measurements of the reactivity feedback from radial core expansion.[6] The FFTF data were also analyzed with NUBOW-3D at HEDL and extended to power-to-flow ratios greater than 1.0. Comparison with the resulting correlation and the SASSYS/SAS4A model also provided verification that the single subassembly representation was in agreement with NUBOW-3D calculations.

Limited validation of the model has also been conducted using the core restraint code NUBOW-3D in conjunction with detailed 3-D temperature, flux, and reactivity worth maps for two small LMRs. [4] The single assembly model agrees with the detailed 3-D calculations of reactivity change during core thermal transients to within 20%. Parameter studies of the dependence of C_2

on core location, load pad stiffness, ACLP location and thermal expansion coefficient show agreement with NUBOW-3D calculations to within a few percent.

Uncertainties

To understand the uncertainties in this simple model of radial expansion reactivity feedbacks, (3), we began by observing that the temperature changes, τ_i, occur at different times during a transient event. τ_2, heating of the ducts, occurs rapidly in response to the heating of the fuel pins with a time lag of about 1 sec. τ_1, heating of the grid plate, occurs much later because most of the system coolant inventory must be heated and then the heavy grid plate must respond to that increased coolant inlet temperature. τ_3, the ring temperature, follows τ_1. Based on this observation we examine the uncertainties of the three terms in the radial expansion reactivity seperately:

$$\Delta R_i = W(LC_i\alpha_i)\Delta T_i \qquad (4)$$

where we have now expressed $\Delta\tau_i = \alpha_i \Delta T_i$ and grouped the terms to represent the neutronic, structural, and thermal terms in the model.

Formal uncertainty analysis of (4) gives.

$$\hat{\Delta R}_i = [\hat{W}^2 + (L\ \hat{C}_i\ \alpha_i)^2 + \hat{\Delta T}_i{}^2]^{\frac{1}{2}} \qquad (5)$$

where $\hat{}$ represents the coefficient of variation, that is the standard deviation divided by the mean value.

Neutronic Uncertainties

$\hat{W}$ is a measure of uncertainty in the calculated value of reactivity change due to a uniform radial expansion of the whole core. It is composed of two parts, uncertainties in the calculational method (normally an eigenvalue difference in multi-group diffusion theory) and uncertainties in the cross section data used in the calculation. For a global parameter such as uniform dilation worth the methods are quite accurate. We have chosen to estimate $\hat{W}$ = 0.10.

Structural Uncertainty

$(L\hat{C}_i\alpha_i)$ formally becomes $[\hat{L}^2 + \hat{C}_i^2 + \hat{\alpha}_i^2]^{\frac{1}{2}}$ if we accept independence of the assembly length, the model for C_i, and the material thermal expansion, α_i. $\hat{L}$ is due to assembly length tolerances, changes in length due to irradiation swelling, and uncertainties in the effective point of contact at the load pads. We estimate $\hat{L} = 0.02$. $\hat{\alpha}$ is the uncertainty in the thermal expansion which we choose to be 0.05.

Uncertainty in the thermal expansion coefficient $\hat{C}_i$ is more involved because of the complexity of (2). It is impractical to do a formal uncertainty expansion of this algebraic expression in part because the uncertainties in the various geometric parameters are not independent. Instead we choose a reference case with nominal values of the parameters, evaluate the derivatives of C_i with respect to those parameters, and estimate the uncertainty on C_i by

$$\hat{C}_i \simeq [\sum_j (\frac{\partial C_i}{\partial \rho_j} (\bar{p})\ \hat{p}_j\ \bar{p}_j)^2]^{\frac{1}{2}} / \bar{C}_i$$

where p_j represents the various dimensionless parameters in the model of C_i, and $\bar{}$ represents the mean value.

This is equivalent to a linear expansion of C_i about its nominal value and independence of the uncertainties of the parameters in the linear model. The linear expansion is valid so long as nonlinearities are not significant over the range of the parameter uncertainty. Our experience with the single assembly model is that this is true provided we avoid values of λ above about 0.001.

Table I gives the parameters, their nominal values, and the uncertainties assumed for those values. The results are:

i	$\hat{C}_i$	$(L\ \dot{C}_i\ \alpha_i)$
1	0.060	0.081
2	0.042	0.068
3	0.157	0.166

Table I Nominal Parameters for the Reference Case and Associated Coefficients of Variation

Parameter	Description	Normal Value	C.O.V.
α	Core half height	.12	.01
β	Core location	.40	.01
γ	ACLP location	.55	.03
ρ	Core radious	.25	.001
λ	Stiffness ratio	.0005	.50
τ_r	Radial thermal gradient	4.40	.30
μ_2	Duct thermal expansion ratio	.7091	.05
μ_3	Ring thermal expansion ratio	.7091	.05
$\varepsilon(\delta)$	ACLP gap	0.0004	.50
$\varepsilon(1)$	TLP gap	.0001	.30
$\eta(\chi)$	Inelastic bow	0.0	-
L	Assembly length	160. (in)	.02
	Thermal expansion ($°F^{-2}$)		
α_1	Grid plate	1.1×10^{-5}	.05
α_2	Duct	7.8×10^{-6}	.05
α_3	Ring	7.8×10^{-6}	.05

Thermal Uncertainties

We are concerned with uncertainties in the grid plate temperature change, ΔT_1, the change in the duct wall temperature rise through the core, ΔT_2, and the change in the ring temperature above the grid plate temperature, ΔT_3.

The grid plate temperature is an integral value in the sense that all of the core outlet coolant is mixed in the top plenum, the pumps, and the inlet plenum before heating the grid plate. Most of the uncertainty is associated with the system heat transfer calculations. We estimate the total coefficient of variation in ΔT_1 to be 0.10.

The duct wall temperature rise uncertainty is primarily associated with the uncertainty in calculating edge flow effects for the assembly. A large data base exists for temperatures in hexagonal fuel assemblies but it is primarily focused on uncertainties in cladding temperatures for peak pins. For this study we have estimated the uncertainty in the duct wall temperature rise to be $\hat{T}_2 = 0.25$. It is quite possible that a more thorough evaluation of existing test data could reduce this number.

The ring temperature is controlled by the bypass flow between the outer shield assemblies and the core barrel. We expect considerable uncertainty in this temperature because of the complex flow path and the low pressure drop of this bypass flow. For this study we estimate $\hat{T}_3 = 0.50$.

Modeling Uncertainty

In addition to the uncertainty associated with the simple model (4) there is a modeling uncertainty, $\hat{M}_i$, which represents the difference between the reactivity change estimated by (4) and the actual reactivity change in a real reactor. This uncertainty assumes that the parameters in (4), W, $(LC_i\alpha_i)$, and ΔT_i are known exactly for a given reactor. Then the difference between ΔR_i as predicted by (4) and the actual radial expansion reactivity is measured by $\hat{M}_i$. One estimate for $\hat{M}_i$ comes from the variations in the reactivity for the simple model and those predicted by the detailed, 3D calculations of the NUBOW code. These have been found to vary by 15-20%. Another source for estimating $\hat{M}$ comes from the comparisons with FFTF measurements. Based on this limited information we choose

$\hat{M}_1$ = 0.1 for the inlet temperature model.

$\hat{M}_2$ = 0.20 for the core temperature rise model

and $\hat{M}_3$ = 0.20 for the ring temperature model.

The total uncertainty in the radial expansion reactivity is then

$$\hat{U} = (\Delta\hat{R}_i^{\,2} + \hat{M}_i^{\,2})^{\frac{1}{2}} = [\hat{W}^2 + (L\ \hat{C}_i\ \alpha)^2 + \Delta\hat{T}_i^{\,2} + \hat{M}_i^{\,2}]^{\frac{1}{2}}$$

Table II summarizes the results of these calculations. It emphasizes that thermal, and to a lesser extent modeling, uncertainties dominate the uncertainty in radial expansion reactivities. The total uncertainty due to grid plate expansion is less than 25% while the uncertainty due to core temperature rise is about 30%. The large uncertainty in the ring temperature coefficient, nearly 60%, is compensated for by the fact that the magnitude of the ring coefficient is normally about 1/10th that of the grid plate and duct coefficients.

While this uncertainty analysis has considerable room for refinement, particularly in the area of temperature uncertainties, it does provide a starting point for estimating the effects of radial expansion uncertainty on inherent safety margins.

Effect of Uncertainties on Inherent Reactor Response

The effect of uncertainty in the radial expansion reactivity feedback on the inherent response to unprotected accidents was investigated using a typical medium-sized LMR design. The reactor core used a heterogeneous design with metallic fuel. The core dimensions and restraint system were identical to those in the previous section. The primary circuit was of the pool type. The main heat sink was a steam generator operating with a superheated steam cycle. The reactivity feedback coefficients were also representative of this reactor type in this size range. The fuel expansion is assumed to be controlled by the motion of the cladding, which is appropriate for irradiated metallic fuel.

The inherent response was determined for an unprotected loss-of-flow, transient overpower, and loss-of-heat-sink accidents. An uncertainty of 30% was used for the radial core expansion reactivity feedback based on the results of the previous section. The key assumption here is that the uncertainty in the duct flat-to-flat temperature difference is not sufficient

Table II Summary of Uncertainties in Radial Expansion Reactivity

Coefficient of Variation	Inlet	Temperature Core Rise	Ring Rise
Dilation Reactivity ($\hat{W}$)	0.1	0.1	0.1
Structure ($L\hat{\alpha}_i C_i$)	0.081	0.068	0.166
Temperature Change ($\hat{\Delta T}_i$)	0.15	0.20	0.50
Model ($\hat{\Delta R}_i$)	0.198	0.234	0.536
Modeling ($\hat{M}_i$)	0.1	0.2	0.2
Total Uncertainty ($\hat{U}$)	0.221	0.308	0.572
Percent of Total			
($\hat{W}$)	20%	11%	3%
($L\hat{\alpha}_i C_i$)	13%	5%	9%
($\hat{\Delta T}_i$)	46%	42%	76%
($\hat{M}_i$)	21%	42%	12%

to cause a loosening of the core at nominal steady-state conditions, with the above-core load pads compacted and a continuous load path out to the restraint ring at the top load pad. For the purposes of this discussion, the emphasis is placed on the short-term safety margin, since the long-term margins are dependent on design-specific details such as the presence of auxiliary cooling systems and steam generator performance during off-normal conditions.

Unprotected Loss-of-Flow Accident

The results for the unprotected loss-of-flow are discussed first, since this accident only involves the uncertainty in the core temperature rise for the early stages of the accident. For an unprotected loss-of-flow started by a loss of off-site power, there is no significant increase in core inlet temperature for the first several hundred seconds, and the restraint ring is essentially fixed in dimension due to the flow coastdown and a time constant typically on the order of a few hundred seconds. As part of the unprotected loss-of-flow accident, the primary and intermediate loop coolant pumps coast down, as well as the steam generator feedwater pumps, with a failure to scram the reactor. This causes the flow through the core to decrease rapidly, while also removing the normal heat sink.

The initial flow coastdown for this accident was set for a six second halving-time; that is, the flow through the core has reduced to 50% of its initial value six seconds after the start of the accident. The subsequent mismatch in power and flow causes the coolant temperature to rise rapidly. For this type of transient, the reactivity feedback from radial core expansion contributes approximately 65-70% of the total negative feedback, and is the dominant mechanism for mitigating the accident consequences. The reactivity feedback at 20 and 40 seconds after the start of the transient are as follows:

Time	Net	Coolant Density	Fuel Axial Expan.	Doppler	Radial Expan.	Control Rod Exp.
20.0 s	-33.05 ¢	10.71 ¢	-4.48 ¢	-3.37 ¢	-31.42 ¢	-4.49 ¢
40.0 s	-44.52 ¢	11.47 ¢	-4.52 ¢	-2.23 ¢	-37.44 ¢	-11.80 ¢

The maximum coolant temperature peaks at 40.0 seconds after the start of the transient, with a peak average coolant temperature of 1030 K in the lead driver assemblies. The coolant saturation temperature is 1210 K at this point, providing a margin of 180 K to coolant boiling.

The conclusion of the uncertainty estimates of the previous section was that an overall uncertainty of 30% was appropriate for the part of the radial core expansion that depended on the core temperature rise. This is simulated by reducing the feedback coefficient 30% and repeating the calculation for the unprotected loss-of-flow. As expected, there is a reduction in the contribution from radial core expansion, to where it only supplied 55-60% of the total negative feedback. The results at 20.0 and 40.0 seconds are as follows:

Time	Net	Coolant Density	Fuel Axial Expan.	Doppler	Radial Expan.	Control Rod Exp.
20.0 s	-28.36 ¢	12.69 ¢	-5.50 ¢	-5.07 ¢	-25.66 ¢	-4.82 ¢
40.0 s	-40.52 ¢	13.55 ¢	-5.54 ¢	-3.92 ¢	-31.15 ¢	-13.46 ¢

For this case, the maximum coolant temperature is higher, peaking at 1070 K at almost 40 seconds into the transient. The higher temperature offsets the loss of negative feedback from radial core expansion, and results in more negative feedback from the other sources. Even though the radial expansion reactivity feedback coefficient was reduced by 30%, the contribution to the net reactivity during the transient is reduced by only 18-20% due to the higher temperatures. The net effect on the margin to coolant boiling is that the margin is reduced by 40 K to 140 K. Considering the large change in the reactivity feedback coefficient for radial core expansion, and that radial core expansion is the dominant source of negative reactivity feedback, the increase in peak coolant temperature is not substantial, with a reduction in the safety margin of 22% for this case.

Unprotected Transient Overpower Accident

The unprotected transient overpower accident is initiated by the uncontrolled withdrawal of a control rod at the nominal control rod speed. This provides a reactivity insertion which is generally on the order of 0.01

\$/sec. For the purposes of this study, a worth of 0.25 \$ is assumed for the initiator, which is also typical of a low burnup-swing design, including uncertainties in the core design and manufacture. There is a failure to scram the reactor, with the remainder of the system continuing to function. As for the LOF, the emphasis is placed on the initial peak in temperatures, since this minimizes the impact of the remainder of the primary and intermediate circuits.

For the unprotected TOP, the power rises as the control rod is withdrawn to a maximum of 138% of nominal power. This occurs at 26 seconds after the start of the accident. During this period, the feedback from radial core expansion accounts for 45-50% of the total negative feedback. The results at 10 and 25 seconds are as follows:

Time	Net	Coolant Density	Fuel Axial Expan.	Doppler	Radial Expan.	Control Rod Exp.
10.0 s	5.31 ¢	1.52 ¢	-0.91 ¢	-2.07 ¢	-3.04 ¢	-0.19 ¢
25.0 s	7.78 ¢	3.92 ¢	-2.39 ¢	-5.56 ¢	-10.72 ¢	-2.47 ¢

As for the LOF, the reactivity feedback from radial core expansion is the dominant source of negative feedback to mitigate the transient. The peak average coolant temperature in the lead drivers occurs at 27 seconds, with a value of 887 K. This is almost 500 K below the coolant saturation temperature of 1340 K. The peak fuel centerline temperature is 1130 K at the same time, which is a rise of 120 K from the nominal steady-state conditions.

Later in the transient, the assumptions connected with the available heat sink become important. For this case, it was assumed that only nominal heat rejection could be maintained at the steam generator. For this reason, after about 70 seconds, the core inlet temperature begins to rise and the reactor attempts to equilibrate the power generation with the heat rejection. The net reactivity becomes negative at 120 seconds, and the power drops to 125% of nominal by 200 seconds. The reactivities at this time are as follows:

Time	Net	Coolant Density	Fuel Axial Expan.	Doppler	Radial Expan.	Control Rod Exp.
200.0s	-1.23 ¢	6.53 ¢	-3.31 ¢	-6.45 ¢	-15.35 ¢	-7.65 ¢

Radial core expansion is still contributing 45% of the negative feedback at this time. The transient will continue until the power generation is back to nominal, with the entire system at a higher temperature.

A reduction of 30% in the reactivity feedback coefficient was also used for this accident. The result is that the power peaks at 146% of nominal, at about 26 seconds into the accident. During this part of the transient, the distribution of reactivity feedback is as follows:

Time	Net	Coolant Density	Fuel Axial Expan.	Doppler	Radial Expan.	Control Rod Exp.
10.0 s	5.93 ¢	1.64 ¢	-0.98 ¢	-2.26 ¢	-2.27 ¢	-0.20 ¢
25.0 s	8.88 ¢	4.55 ¢	-2.78 ¢	-6.43 ¢	-8.69 ¢	-2.77 ¢

In comparison with the previous case, the peak coolant temperature is 900 K at 29 seconds, an increase of 13 K. The peak fuel centerline temperature is 1150 K, an increase of 20 K. Such minor changes are again due to the compensating effects of the other sources of negative reactivity feedback achieved with slightly higher core temperatures. For this case, the radial core expansion contributes only 38-40% of the negative reactivity feedback.

As the transient continues, the net reactivity becomes negative at 120 seconds, with the power dropping to 131% of nominal at 200 seconds. The reactivity feedback at this point is as follows:

Time	Net	Coolant Density	Fuel Axial Expan.	Doppler	Radial Expan.	Control Rod Exp.
200.0s	-1.28 ¢	7.86 ¢	-3.99 ¢	-7.77 ¢	-13.08 ¢	-9.30 ¢

At this time, radial core expansion is contributing 37% of the negative feedback. As before, the transient will continue until the power generation returns to the nominal heat rejection capability of the steam generator.

The overall effect on the inherent response to the TOP accident is less than for the LOF, since radial core expansion contributes a smaller proportion of the total negative reactivity feedback. The smaller impact on the results is not surprising, with the 30% reduction in feedback causing an 18% increase in the peak power and in the fuel and coolant temperature changes. This has no significant impact on the margin to coolant boiling, which is almost 500 K, but it does cause a reduction of approximately 10% in the margin to fuel melting.

<u>Unprotected Loss-of-Heat-Sink</u>

The unprotected loss-of-heat-sink accident is initiated by a total loss of heat rejection capability at the steam generator with a failure to scram the reactor. As a result, the effect of losing the steam generator is not reflected at the core inlet until at least 30 seconds after the beginning of the transient. This accident is particularly sensitive to the uncertainty in the part of the radial core expansion that is proportional to the core inlet temperature. As in the previous cases, an uncertainty of 30% will be assigned for the transient, although if the uncertainty in core temperature rise were included as an independent variation, a value of 40% would be more appropriate. However, the time scale for these two effects is much different, so the entire amount is not involved in the calculation until late in the transient. As will be shown, the use of a more detailed model for calculating radial core expansion is far more important than the uncertainty associated with the core inlet temperature.

The inlet temperature begins increasing at approximately 30 seconds into the transient. The effect of this increase is reflected in rapid, but small, increases in the load pad temperatures, which in turn introduces significant negative feedback to reduce the power. The expansion of the grid plate, or core support plate, occurs much more slowly due to the large thermal time constant associated with this structure. During the early stages of the transient, for the first 100 seconds or so, the power is decreasing almost as rapidly as the inlet temperature is increasing. The net effect is that there is very little change in the peak coolant temperature, although the fuel centerline temperatures are decreasing.

The loss-of-heat-sink transient is characterized by two distinct phases, one where the subassemblies are loaded at the grid plate and at the above-core load pad region (compacted) and at the top load pad region (pushing against any radial blankets, shields, and/or reflectors out to the restraint ring), and the second phase where this core loading state is changing. The second phase is entered once the power has been reduced sufficiently to decrease the thermal bending of the assembly, causing it to lose the loading at either of the load pad regions, or both. The most pessimistic result for this phase of the transients is obtained by assuming that the assemblies remain as compacted as possible. This generally results in the above-core load pad region remaining compacted early in the second phase, and then the top load pads prevent further compaction and gaps occur between the above-core load pads. Both of these conditions in the second phase are also characterized by greatly different reactivity feedback. As is shown below, this causes the power to reduce very rapidly after the second phase is entered, with the power dropping almost to decay heat levels. For this calculation of the unprotected loss-of-heat-sink, the transition occurs at 135 seconds after the start of the transient.

At this time in the transient, the reactivity contributions are as follows:

Time	Net	Coolant Density	Fuel Axial Expan.	Doppler	Radial Expan.	Control Rod Exp.
135.0s	-7.36 ¢	6.66 ¢	-2.28 ¢	-1.77 ¢	-7.92 ¢	-2.04 ¢

At this point in the transient, radial core expansion is providing about 55% of the negative feedback. The peak coolant temperature has risen to 826 K, an increase of 12 K. The fuel centerline temperature has fallen to 952 K from 1010 K at steady-state due to the drop in power to 62% of nominal. After this, the loading conditions change, with the subassembly no longer pushing out at the top load pads. As described, this substantially changes the feedback from radial core expansion.

By 200 seconds, the power has dropped to 30% of nominal, with a peak coolant temperature of 808 K, a peak fuel centerline temperature of 873 K, and a core inlet temperature of 752 K. At 400 seconds, the power has dropped to 2.4% of nominal, with a core inlet temperature of 780 K and a peak coolant temperature in the lead drivers of 785 K. The distribution of the reactivity feedback at these times is as follows:

Time	Net	Coolant Density	Fuel Axial Expan.	Doppler	Radial Expan.	Control Rod Exp.
200.0s	-16.94 ¢	8.70 ¢	-2.60 ¢	-0.58 ¢	-22.47 ¢	-0.01 ¢
400.0s	-39.56 ¢	9.64 ¢	-2.50 ¢	1.20 ¢	-55.81 ¢	7.91 ¢

As the results demonstrate, radial core expansion is contributing from 90-95% of the negative feedback at this stage of the accident. The system is also far from equilibrium, with a net negative feedback of almost 40 c. The course of the remainder of the transient is dependent on the available heat sinks, and is not considered here.

Reducing the reactivity feedback from radial core expansion by 30%, similar results are obtained, but at higher temperatures. The transition from one core loading state to the next occurs at 137 seconds into the transient, at a power level of 61% nominal. The peak fuel centerline temperature has been reduced to 957 K, with a peak coolant temperature of 832 K and a core inlet temperature of 720 K. The reactivity feedback at this time is as follows:

Time	Net	Coolant Density	Fuel Axial Expan.	Doppler	Radial Expan.	Control Rod Exp.
137.0s	-6.94 ¢	7.63 ¢	-2.69 ¢	-2.36 ¢	-6.82 ¢	-2.71 ¢

Radial core expansion is contributing 47% of the negative feedback at this time. After this, the power again reduces rapidly to 39% at 200 seconds and 4.3% at 400 seconds. At 400 seconds, the core inlet temperature is 790 K, with a peak coolant temperature of 798 K and a peak fuel centerline temperature of 807 K. The 30% reduction in radial expansion feedback has made a minor increase in the overall system temperature. The reactivity feedback at these times is as follows:

Time	Net	Coolant Density	Fuel Axial Expan.	Doppler	Radial Expan.	Control Rod Exp.
200.0s	-10.44 ¢	9.67 ¢	-3.17 ¢	-2.06 ¢	-13.96 ¢	-0.91 ¢
400.0s	-25.07 ¢	11.03 ¢	-3.14 ¢	-0.07 ¢	-40.57 ¢	7.68 ¢

In comparison to the nominal feedback case, there is a substantial change in the net feedback, and in the feedback from radial core expansion, but this is not reflected in any of the system temperatures due to the low power level with the net reactivity being substantially negative. In summary, the effect on the transient with a 30% change in radial expansion reactivity feedback is minimal. The major effect is the ability to determine when the core loading state changes (1,6), as this controls the course of the unprotected loss-of-heat-sink transient. It must be emphasized that all of these calculations assumed that the core loading conditions at steady-state were not affected by the variations in the duct temperatures. If the core was not "locked" at steady-state, the results can be considerably different.

Conclusions

A single assembly bowing model of core radial expansion has been shown to be adequate for evaluating inherent safety margins in response to unprotected transients in advanced LMRs. The model has both experimental and numerical validation, and is capable of quantifying the effect of the uncertainties associated with the parameters which control the reactivity feedback from radial core expansion. Application of uncertainty analysis, in conjunction with the radial core expansion model, has led to the conclusion that the magnitudes of the estimated uncertainties are on the order of 30% to 40%. The use of SASSYS/SAS4A to analyze unprotected LOF, TOP, and LOHS transients demonstrated that this level of uncertainty does not significantly reduce the safety margins for a typical advanced LMR with metallic fuel and of inherently-safe design. However, any variation which causes the core to have an indeterminate geometry at nominal steady-state conditions may have a significant impact on the transient results.

References

1. R. A. Wigeland, "Effect of a Detailed Radial Core Expansion Reactivity Feedback Model on ATWS Calculations Using SASSYS/SAS4A," Trans. Am. Nucl. Soc., 53, p. 303 (November 1986).

2. A. M. Tentner et al., "The SAS4A LMFBR Whole Core Accident Code," Proc. Int. Mtg. Fast Reactor Safety, p. 989, Knoxville, TN (1985).

3. F. E. Dunn et al., "The SASSYS-1 LMFBR Systems Analysis Code," Proc. Int. Mtg. Fast Reactor Safety, p. 999, Knoxville, TN (1985).

4. T. J. Moran, "Core Restraint Design for Inherent Safety," to be presented at the Joint ASME/ANS Nuclear Power Conference, Myrtle Beach, South Carolina, April 17-20, 1988.

5. T. J. Moran, "Core Restraint Contributions to Radial Expansion Reactivity," Proc. 1986 Joint ASME/ANS Nuclear Power Conference, p. 454, Philadelphia, PA (July 1986).

6. R. A. Wigeland, "Comparison of the SASSYS/SAS4A Radial Core Expansion Reactivity Feedback Model and the Empirical Correlation for FFTF," Trans. Am. Nucl. Soc., 55, p. 423 (November 1987).

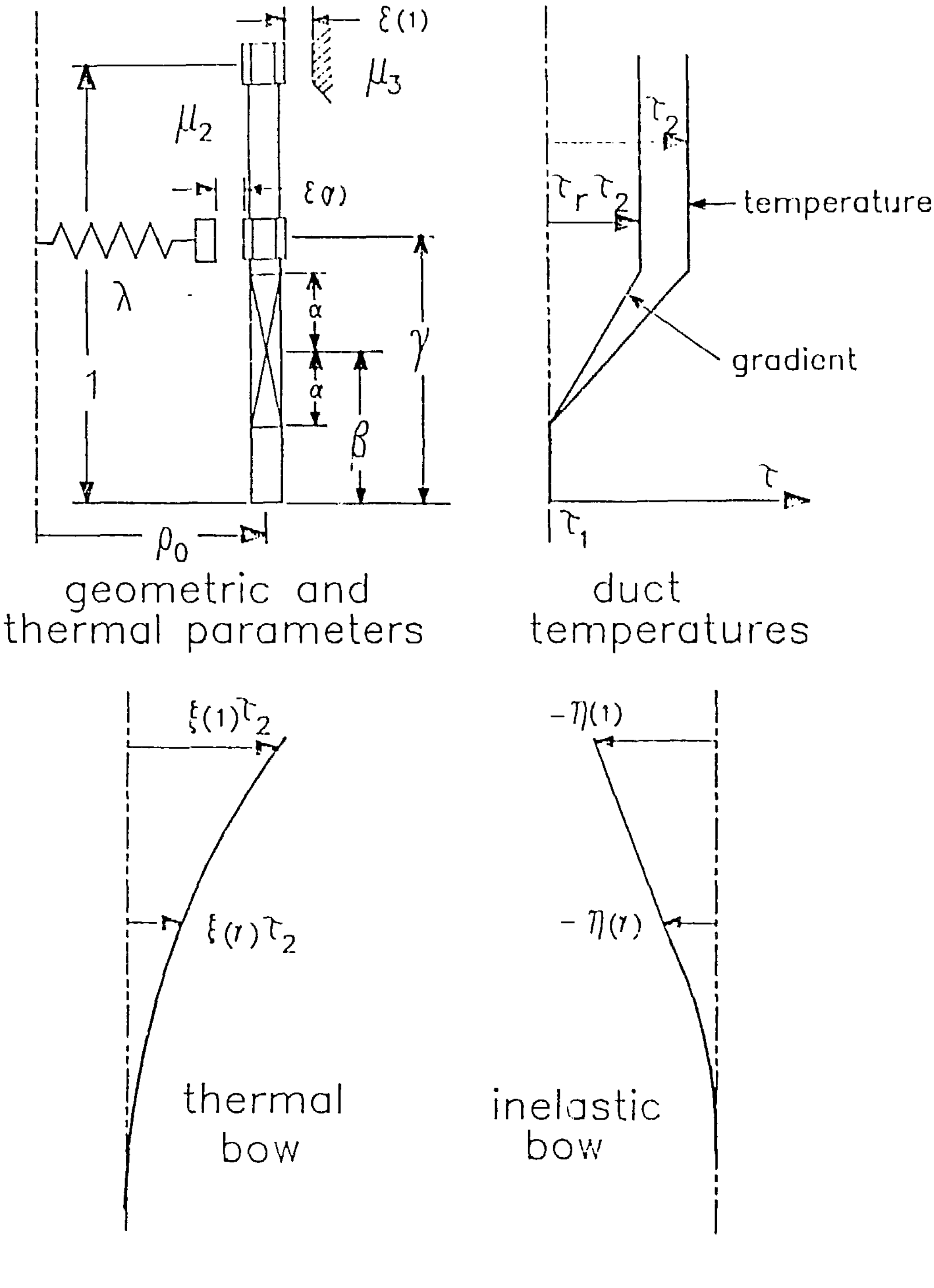

Fig. 1. Dimensionless Parameters Used in the Single Assembly Model.