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CORE RESTRAINT DESIGN FOR INHERENT SAFETY

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ABSTRACT

A simple analytical model is developed of core radial expansion for a fast reactor using a limited-free-bow core restraint design. Essentially elementary beam theory is used to calculate the elastic bow of a driver assembly 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 model is used to show the relative importance of grid plate temperature, core temperature rise, and restraint ring temperature in the inherent response of a limited-free-bow core restraint system to thermal transients. It is also used to explore how changes on the design parameters will effect this inherent core expansion. Limited verification of the model using detailed 3-D core restraint calculations is presented.

INTRODUCTION

Radial expansion of the core due to rising temperatures provides a major portion of the negative reactivity needed to ensure passive shutdown of a Liquid Metal Fast Reactor (LMR) during unprotected transients (1). The magnitude of the radial thermal expansion of the core is dependent on the details of the mechanical support of the core as well as thermal-hydraulic features of the design. Previous work has demonstrated the superiority of a limited-free-bow (LFB) core restraint design in providing radial expansion of the core during critical periods of a transient when the power-to-flow ratio (P/F) of the core is rising (2). The purpose of this work is to provide an analytical model of core radial expansion as a basis for optimizing this dominate inherent feedback mechanism.

The LFB core restraint design relies on the core support plate for radial restraint at the coolant inlet nozzle end of the assemblies. Load pads are provided on the assembly ducts just above the core (ACLP) to provide spacing between assemblies and to carry the loads associated with restraining bow of the assemblies. At the top of each assembly a second load pad (TLP) is provided for same reason. A circumferential ring is provided outside the outer row of removable assemblies at the TLP elevation to prevent excessive radial expansion. An optional ring may also be provided at the ACLP elevation.

As the P/F ratio rises, thermal gradients across the ducts cause bowing of the assembly, predominately in the radial direction . While the magnitude (and direction) of the thermal bow varies from assembly to assembly, it is generally proportional to P/F for each assembly and maximum near the core radial boundary. When this bow is sufficiently large the TLP restraint ring prevents further radial expansion at that elevation and causes compaction of the core at the ACLP elevation. When the P/F ratio is sufficiently large so that the thermal bow has squeezed out the available gaps at the ACLP plane the restraint system is said to be "locked-up". Further increases in P/F causes more thermal bowing of the assemblies but the ACLP and TLP contact states do not change. The ACLP plane stays compacted and the TLP plane remains expanded to the TLP ring. The additional thermal bow is accommodated by elastic bowing of the assembly which results in an "s-shape" for the assembly with the TLP, ACLP and nozzle points fixed. The assembly between the nozzle and the ACLP expands while the portion between the ACLP and TLP bows inward.

Analysis of the position of assemblies for a LFB core restraint design at P/F below lock-up is a complex problem. It involves 3-D calculations of the interaction of bowing assemblies and may be partially indeterminate. The essence of the problem is that bowing reaction forces at the load pads cause displacements of neighboring load pads including closing and opening gaps between pads. As a result the bowing of each assembly is coupled with the bowing, displacement and rotation of all assemblies.

Lock-up normally occurs at P/F of between 0.5 and 0.8. Several factors influence lock-up including designed gaps within the restraint ring, creep and swelling of the ducts, manufacturing uncertainties in load pad dimensions, and friction at the load pads. The design constraints are that the core be sufficiently loose at refueling temperatures to allow removal of assemblies and that the core be sufficiently tight at P/F=1 so that assemblies cannot move. While the detailed determination of when lock-up occurs is also a complex problem involving most of the complications associated with assembly positions below lock-up, the salient feature of LFB core restraint design is that lock-up must occur. This is because the thermal bow increases monotonically with P/F and the available space is restricted by the restraint ring.

Above lock-up, the analysis of the position of core assemblies is greatly simplified. Since three points on the assembly are fixed within the array (nozzle, ACLP and TLP) the position of the core is given by the thermal expansions of the grid plate, ACLP plane, TLP plane, and the individual bowing of the assembly. The only coupling is through compression of the ACLP plane and since the gaps at the ACLP are no longer changing this compression is a linear function of the bowing force.

In this paper we develop a simplified model of core radial expansion for an LMR with a LFB core restraint in those bowing regimes above lock-up. Radial expansion is expressed as a linear function of three characteristic temperatures, the coolant inlet temperature, T_i, the core temperature rise, ΔT, and the restraint ring temperature T_r. The coefficients of these temperatures are given as functions of the material properties, core design dimensions, and the ratio of radial thermal gradient to axial core temperature rise. We examine the influence of various design parameters on core radial expansion and discuss the implications of various design choices. Finally, the simplified model is compared with detailed calculations of radial expansion reactivity using the NUBOW-3D code for two small LMRs (3).

SINGLE ASSEMBLY ANALYTICAL MODEL

A simple analytical model is developed of core radial expansion for a fast reactor using a LFB core restraint design. The model is restricted to those bowing regimes where the ACLP plane 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 TLP of the outer driver assemblies are restrained from further radial expansion by continuous load paths to the TLP restraint ring. The model considers a single driver assembly at the core boundary. This approximation is deemed valid due to the fact that the maximum displacement reactivity worths and maximum temperature gradients occur at the core boundary. Also, the maximum displacements due to thermal dilation of the ACLP and thermal expansion of the grid plate occur at this location. For these reasons, core radial displacement of such a driver assembly is a good measure of core dilation for the whole core.

The parameters used in constructing the model are shown schematically in Fig. 1. The total length of the assembly from the nozzle support to the TLP is L. This distance is used to scale all the dimensions of the model so that the axial location of the TLP relative to the nozzle support is 1. The distance from the support to the ACLP is γ and the distance from the support to the midcore is β. The height of the core is 2α.

The model treats the assembly as a simple Bernoulli-Euler beam supported at the nozzle end with either a pinned or cantilever support. The axial coordinate of the beam is x=0, the mid core at $x=\beta$, the ACLP at $x=\gamma$ and the TLP at x=1. $\rho(x,T)$ is the radial position of the beam axis at axial position x and temperature T. At the TLP the beam is restrained from radial outward displacement by a rigid ring. The radial position of that ring is $\rho_r(1,T)$. At the ACLP, the beam is restrained from radial inward displacement by a flexible restraint representing the ACLP plane of the interior assemblies. The position of this restraint is $\rho_r(\gamma,T)$ prior to any elastic compression $\rho_r(0,T)$ designates the grid plate restraint. Gaps are allowed to exist between the nominal straight position of the assembly and the two restraints. These are designated $\varepsilon(\gamma)$ and $\varepsilon(1)$ with a positive value indicating a gap at x=1 and an interference at $x=\gamma$, i.e., positive gaps represent a radial outward displacement of the restraint. All other displacements are also positive in the radial outward direction.

Restraint forces acting at the ACLP and TLP are represented by $\sigma(\gamma)$ and $\sigma(1)$ which are dimensionless

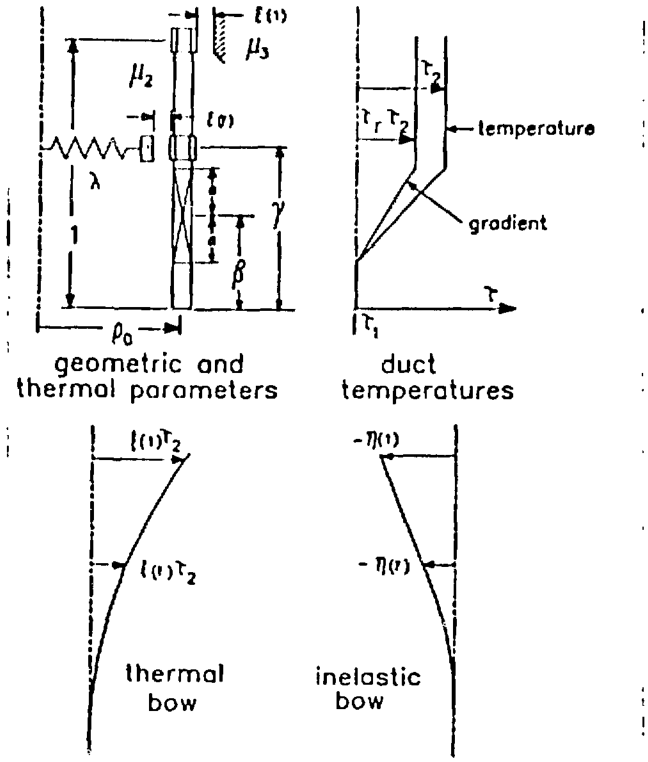

Fig. 1. Dimensionless Parameters Used in the Single Assembly Model

forms normalized using the assembly length and the ACLP stiffness

$$(\text{e.g } \sigma(\gamma) = f(\gamma)/KL).$$

As a result of this definition $\sigma(\gamma)$ is also the dimensionless magnitude of the compression of the ACLP plane. The TLP does not compress in this model.
A dimensionless stiffness ratio is required to relate the relative stiffness of the beam model of the assembly, EI, to the ACLP plane stiffness, K. This parameter is

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

It is convenient to define dimensionless temperatures by multiplying temperatures by the coefficient of thermal expansion of the corresponding material. Thus τ_1, the dimensionless support plate temperature, is the actual temperature times the coefficient of thermal expansion of the support plate. The temperature of the restraint ring is $\tau_1 + \tau_3$ where τ_3 is the temperature difference between the restraint ring and the support plate times the coefficient of thermal expansion of the ring. The assembly temperature is τ_1 from the inlet nozzle to the bottom of the core and rises linearly to $\tau_1 + \tau_2$ at the top of the core (τ_2 is the core temperature rise times the duct coefficient of thermal expansion). The duct temperature remains at $\tau_1 + \tau_2$ from the top of the core to the TLP, Fig. 1. Dimensionless thermal expansion coefficients, μ_2 and μ_3, are defined by dividing by the expansion coefficient of the duct and the ring, respectively, by the grid plate coefficient.

A transverse temperature gradient exists across the assembly and has a value of 0 at the bottom of the core and a value $\tau_r\tau_2$ at the top of the core. The variable τ_r is the ratio of the dimensionless transverse temperature gradient to the dimensionless temperature rise through the core. The transverse temperature gradient causes a bowing of the assembly which can be calculated as $\delta_T(x)$ using simple beam theory. $\delta_T(x)$ satisfies the equation:

$$\delta_T''(x) = 0 \qquad 0 < x < \beta-\alpha$$
$$= -\tau_r\tau_2(x-\beta+\alpha)/2\alpha \qquad \beta-\alpha < x < \beta+\alpha$$
$$= -\tau_r\tau_2 \qquad \beta+\alpha < x < 1$$

and the boundary conditions:

$$\delta_T(o) = 0$$
$$\delta_T'(o) = 0.$$

We are concerned with the values of δ_T evaluated at the TLP, $\delta_T(1)$, at the ACLP, $\delta_T(\gamma)$, and at the core midpoint, $\delta_T(\beta)$.

$$\delta_T(1) = -\left[\frac{1}{2}(1-\beta)^2 + \frac{1}{6}\alpha^2\right]\tau_r\tau_2 \equiv \xi(1)\tau_2 \qquad (1)$$

$$\delta_T(\gamma) = -\left[\frac{1}{2}(\gamma-\beta)^2 + \frac{1}{6}\alpha^2\right]\tau_r\tau_2 \equiv \xi(\gamma)\tau_2 \qquad (2)$$

$$\delta_T(\beta) = -\frac{1}{12}\alpha^2\tau_r\tau_2 \equiv \xi(\beta)\tau_2 \qquad (3)$$

The function $\xi(x)$ is a thermal bow measure where we have factored out the dependence on τ_2. It is positive in the radial out direction for a negative transverse temperatures gradient, the usual case, Fig. 1.

In addition to thermal bow, the assembly may have a bow deformation due to irradiation enhanced creep and swelling. We designate this inelastic deformation $\eta(x)$ positive in the radial out direction. For a creep dominated core $\eta(x)$ will generally be a negative valued function, while for a swelling dominated core it will be positive.

Now the problem remaining is to calculate the elastic bowing of the assembly necessary to satisfy the compatibility conditions imposed by thermal expansion of the grid plate, ACLP plane and restraint ring, the thermal bow and inelastic bow of the assemblies, the gaps, and the compression of the ACLP plane due to the force generated by the restrained assembly.

The thermally expanded position of the grid plate at initial radius ρ_o is:

$$\rho_r(0,T) = \rho_o[1 + \alpha_1 T_1], \qquad (4)$$

That of the restraint ring is:

$$\rho_r(1,T) = [\rho_o + \varepsilon(1)]\,[1 + \alpha_3(T_1 + T_y)] \qquad (5)$$

and the free thermal expansion of the ACLP plane (without regard for compression of the plane due to bowing forces) is:

$$\rho_r(\gamma,T) = [\rho_o + \varepsilon(\gamma)]\,[1 + \alpha_2(T_1 + \Delta T)] \qquad (6)$$

The radial position of the assembly prior to its elastic deformation is given by the nozzle position plus the thermal and inelastic bow:

$$\psi(x,T) = \rho_o[1 + \alpha_1 T_1] + \xi(x)\alpha_2\Delta T + \eta(x) \qquad (7)$$

The dimensionless compression of the ACLP plane is:

$$(f(\gamma)/K)/L = \sigma(\gamma).$$

The net interference at the load pads is given by:

$$\zeta(1,T) \equiv \rho_r(1,T) - \psi(1,T)$$
$$\zeta(\gamma,T) \equiv \rho_r(\gamma,T) - \psi(\gamma,T) \qquad (8)$$

Elementary beam theory states that the second derivative of the elastic bowing displacement is equal to the bending moment of the beam divided by the section modulus, EI. If L $\delta(x)$ is the elastic bow, then in dimensionless form

$$\delta''(x) = \frac{M(x)}{EIL^3} \qquad (9)$$

where $M(x)$ is the bending moment. Since the only restraint forces are at the nozzle support, the ACLP and the TLP, $M(x)$ is linear in x in both the domain $0\le x\le\gamma$ and the domain $\gamma\le x\le 1$. Thus $\delta(x)$ is a cubic in x in each of the two domains and 8 constants of integration are required. In addition, the ACLP reaction force $\sigma(\gamma)$ must be evaluated. The following conditions allow evaluation of these 8 integration constants and the reaction force.

Compatibility at the grid plate requires

$$\delta(0) = \rho_r(0,T) - \psi(0,T) = 0.$$

Compatibility at the TLP restructuring requires that the elastic bow compensate for the net interference:

$$\delta(1) = \zeta(1,T) \qquad (10)$$

Similarly, at the ACLP the elastic bow minus the compression of the ACLP plane must equal the net interference.

$$\delta(\gamma) - \sigma(\gamma) = \zeta(\gamma,T) \qquad (11)$$

At the TLP there is an additional boundary condition of zero moment:

$$\delta''(1) = 0.$$

At the nozzle we consider two cases, either a pinned nozzle support which requires no moment:

$$\delta''(0) = 0$$

or a cantilever support which requires zero slope:

$$\delta'(0) = 0.$$

Continuity conditions are also needed at the ACLP. These are that $\delta(x)$, $\delta'(x)$, $\delta''(x)$ be continuous at $x = \gamma$ and that $\delta'''(x)$ have a step discontinuity proportional to the restraint force:

$$\delta'''(\gamma-) - \delta'''(\gamma+) = \sigma/\lambda.$$

With this notation the solution of (8) subject to the boundary conditions and continuity conditions takes a reasonably simple form. In the case of a pinned support:

$$\delta_p(\beta,T) = \frac{\beta}{\gamma}\zeta(\gamma,T) + \beta(\phi-1)\left[\frac{\zeta(\gamma,T)}{\gamma} - \zeta(1,T)\right] \quad (12)$$

The first term, $\frac{\beta}{\gamma}\zeta(\gamma,T)$, represents a rotation of the assembly about the nozzle pinned support sufficient to account for the net interference at the ACLP. The term $\left[\frac{\zeta(\gamma,T)}{\gamma} - \zeta(1,T)\right]$ is the net interference at $x = 1$ after this rotation. The term $\beta(\phi-1)$ is the elastic displacement at $x=\beta$ 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.

$$\phi = \frac{\frac{2-\gamma}{1-\gamma} - \frac{\beta^2}{\gamma(1-\gamma)}}{2 + \frac{6\lambda}{\gamma^2(1-\gamma)^2}} \quad (13)$$

In the case of a cantilever support the assembly cannot rotate freely so the solution is a superposition of the influence at β due to the net interferences at $x = \gamma$ and $x = 1$.

$$\delta_c(\beta,T) = \theta_\gamma\zeta(\gamma,T) + \theta_1\zeta(1,T) \quad (14)$$

where

$$\theta_\gamma = \frac{\beta^2[2(3\gamma^2(3-\gamma)(3-\beta))]}{\gamma^3(4-\gamma)(1-\gamma)^2 + 12\lambda} \quad (15)$$

is the elastic displacement at β due to the net interference at $x = \gamma$ and

$$\theta_1 = \frac{\beta^2[-\gamma^2(3-\gamma)(3\gamma-\beta) + 2\gamma^3(3-\beta)]}{\gamma^3(4-\gamma)(1-\gamma)^2 + 12\lambda} \quad (16)$$

is the elastic displacement of β due to the net interference at $x = 1$.

Note that since the elastic solution is analytic in the region $0 \le x \le \gamma$, $\delta_p(x,T)$ is obtained from (12) by simple substitution of x for β, in (12) and (13). The same is true for the cantilever case. $\delta_c(x,T)$ in in the region $0 \le x \le \gamma$ is obtained by substitution of x for β in (14), (15) and (16). We will, however, only make use of $\delta(\beta,T)$ in this analysis.

As part of the solution we obtain the reaction forces necessary to maintain the bowed state. For the case of the pinned support we have

$$\sigma(\gamma) = \frac{3\lambda[\delta(\gamma,t)-\gamma\delta(1,T)]}{3\lambda + \gamma^2(1-\gamma)^2}$$

and the condition that the assembly remain in contact with the ACLP plane of the internal assemblies is

$$\delta(\gamma,T) > \gamma\delta(1,T).$$

For the cantilever support the two reaction forces are:

$$\sigma(\gamma) = \frac{6\lambda[-\gamma^2(3-\gamma)\zeta(1,T)-2\zeta(\gamma,T)]}{[\gamma^3(4-\gamma)(1-\gamma)^2+12\lambda]}$$

$$\sigma(1)\ \frac{-6\lambda[\gamma^2(3-\gamma)\zeta(\gamma,T)-2\gamma^3\zeta(1,T)]}{[\gamma^3(4-\gamma)(1-\gamma)^2+12\lambda]}$$

and the conditions that must be met to satisfy the assumption on contact at the ACLP and TLP are:

$$\sigma(\gamma) > 0 \Rightarrow \zeta(\gamma,T) > \tfrac{1}{2}\gamma^2(3-\gamma)\zeta(1,T)$$

$$\sigma(1) > 0 \Rightarrow \zeta(\gamma,T) > \frac{2\gamma}{(3-\gamma)}\zeta(1,T).$$

The radial position of the core midplane is then

$$\rho(\beta,T) = \delta(\beta,T) + \psi(\beta,T) \quad (17)$$

where $\psi(\beta,T)$ is given by (4) and $\delta(\beta,T)$ is given by (12) for a pinned support or (14) for a cantilever support. It will prove useful to express the temperature dependence of $\rho(\beta,T)$ explicitly. To do this we define the temperature coefficients of the net interference at the load pads by

$$\zeta(1,T) \equiv \sum_{i=0}^{3} \zeta_i(1)\tau_i, \quad (18)$$

where $\zeta_i(1)$ are the temperature coefficients of the net interference at the TLP defined by (8) through the use of (5) and (7). In particular:

$$\zeta_0(1) = \varepsilon(1) - \eta(1),$$

$$\zeta_1(1) = \mu_3\varepsilon(1) - (1-\mu_3)\rho_0,$$

$$\zeta_2(1) = -\xi(1),$$

$$\zeta_3(1) = \rho_0 + \varepsilon(1), \text{ and}$$

$$\tau_0 = 1.$$

Similarly the temperature coefficients of the net interference at the ACLP, $\zeta_i(\gamma)$, are defined by

$$\zeta(\gamma,T) \equiv \sum_{i=0}^{3} \zeta_i(\gamma)\tau_i, \quad (19)$$

where

$$\zeta_0(\gamma) = \varepsilon(\gamma) - \eta(\gamma),$$

$$\zeta_1(\gamma) = \mu_2\varepsilon(\gamma) - (1-\mu_2)\rho_0,$$

$$\zeta_2(\gamma) = \rho_0 + \varepsilon(\gamma) - \xi(\gamma), \text{ and}$$

$$\zeta_3(\gamma) = 0.$$

We also define temperature coefficients for the free bow position of the core midplane by

$$\psi(\beta,T) \equiv \sum_{i=0}^{3} \psi_i(\beta)\tau_i,$$

where

$$\psi_0(\beta) = \rho_0 + \eta(\beta),$$

$$\psi_1(\beta) = \rho_0,$$

$$\psi_2(\beta) = \zeta(\beta), \text{ and}$$

$$\psi_3(\beta) = 0.$$

This notation allows us to write

$$\rho(\beta,T) = \sum_{i=0}^{3} C_i \tau_i, \qquad (20)$$

where C_i are the temperature coefficients of the radial expansion at the core midplane. They are obtained by reference to (17), (18), (19), (7), and either (12) for the pinned support case or (14) for the cantilever support case. In particular

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

for the pinned case or

$$C_i = \phi_\gamma \zeta_i(\gamma) - \phi_1 \zeta_i(1) + \psi_i,$$

for the cantilever case.

PARAMETER DEPENDENCE

One of the advantages of the single assembly model of core radial expansion is that its analytic form allows examination of the dependence of radial expansion on a variety of design parameters. In this section we explore that dependence.

The algebraic expressions involved in defining the core radial expansion temperature coefficients, C_i, are rather complex. This will necessitate graphical exploration of parameters dependence in some cases. To accomplish this we will define a reference core by choosing reference values of the various dimensionless parameters and establish a range of values over which to vary those parameters.

Table A list the reference values for the parameters of the reactor we will study. The reference values correspond to a moderate size LMR with an above core plenum using ferritic stainless steel for assembly ducts and restraint ring and using austenitic stainless steel for the support plate. Table B contains the calculated parameters and coefficients for the reference case.

We choose the length of the assembly to be 170 in., the coefficient of thermal expansion of the ferritic duct to be 0.7×10^{-5} °F^{-1} and that of the austenitic support plate to be 1×10^{-5} °F^{-1}. For the reference case the rate of core dilation due to grid plate temperature rise is

$$\frac{\partial r}{\partial T_1} = L\frac{\partial \rho(\beta)}{\partial T_1} = LC_1\alpha_1$$

$= 0.428 \times 10^{-3}$ in./°F for a pinned nozzle support

$= 0.452 \times 10^{-3}$ in./°F for a cantilever nozzle support.

Similarly, the rate of core dilation due to temperature rise through the core is

Table A. Dimensionless Design Parameters for the Reference Reactor in the Parameter Study

Parameter	Reference Value
Core Half Height, α	0.13
Midcore Axial Location, β	0.40
ACLP Axial Location, γ	0.55
Core Radius, ρ_0	0.30
ACLP Gap, $\varepsilon(\gamma)$	0.0
TLP Gap, $\varepsilon(1)$	1.0×10^{-3}
Core Stiffness Ratio, γ	5.0×10^{-4}
Thermal Gradient Ratio, τ_r	-4.40
Mid Core Inelastic Bow, $\eta(\beta)$	0.0
ACLP Inelastic Bow, $\eta(\lambda)$	0.0
TLP Inelastic Bow, $\eta(1)$	0.0
Grid Plate Temperature, τ_1	5.94×10^{-3}
Core Temperature Rise, τ_2	1.76×10^{-3}
Ring Temperature Rise, τ_3	1.56×10^{-3}
Duct Thermal Exp. Ratio, μ_2	0.71
Ring Thermal Exp. Ratio, μ_3	0.71

Table B. Calculated Parameters for the Reference Reactor Design

Parameter	Reference Value
Core Thermal Bow, $\zeta(\beta)$	0.0062
ACLP Thermal Bow, $\zeta(\gamma)$	0.0619
TLP Thermal Bow, $\zeta(1)$	0.8044
Pinned Support:	
ϕ	1.2571
$\sigma(\gamma)$	0.0239
C_0	0.2999
C_1	0.2291
C_2	0.3066
C_3	-0.0310
Cantilever Support:	
ϕ_γ	0.7501
ϕ_1	-0.0802
$\sigma(\gamma)$	0.0245
$\sigma(1)$	-0.0245
C_0	0.2999
C_1	0.2415
C_2	0.2493
C_3	-0.0241

$$\frac{\partial r}{\partial \Delta T} = LC_2\alpha_2 = 0.407 \times 10^{-3} \text{ in./°F pinned}$$

$$= 0.331 \times 10^{-3} \text{ in./°F cantilevered.}$$

The effect of increasing restraint ring temperature is

$$\frac{\partial r}{\partial T_r} = LC_3\alpha_3 = -0.041 \times 10^{-3} \text{ in./°F pinned}$$

$$= -0.032 \times 10^{-3} \text{ in./°F cantilevered.}$$

Clearly the pinned support gives a greater radial expansion for increasing grid plate and core temperature rise. Increasing the restraint ring temperature causes decreased core dilation but the magnitude is small.

Material Property Effects

The coefficients of thermal expansion of the grid plate, assembly, and restraint ring play a central role in core dilation. They appear in the dimensionless parameters of the single assembly model in two ways. First, the dimensionless temperatures, τ_i are linearly dependent on the corresponding thermal expression coefficient; e.g.

$$\tau_i \equiv \alpha_i T_i .$$

Secondly the two thermal expansion ratios, μ_2 and μ_3 appear in the algebraic expressions for the grid plate thermal dilation coefficient C_1. Thus the fast temperature dilation is directly proportional to the coefficient of thermal expansion of the duct material through τ_2. The inlet temperature dilation is directly proportional to the grid plate thermal expansion via τ_1 but is also linearly proportional to both μ_2 and μ_3. Nearly a 40% increase in the inlet temperature dilation coefficient can be obtained by changing the duct material from a ferritic steel to an austenitic steel.

Selection of a low swelling alloy will not directly affect the temperature response of the core, $\eta(\beta)$, $\eta(\gamma)$, and $\eta(1)$ contribute only to C_0, but will effect the lock-up criteria.

Core Location

The core location also plays a strong role. Figure 2 shows the dependence of the temperature coefficients on the location of the core within the assembly, β, assuming that the ACLP is not moved relative to the core. Raising the core gives a nearly linear increase in the fast temperature dilation coefficient with the possibility of nearly doubling the value of C_2 over reasonable ranges of core location, β. It does not significantly effect the inlet temperature dilation coefficient, C_1, but does affect the restraint ring dilation coefficient, C_3, particularly for a high core.

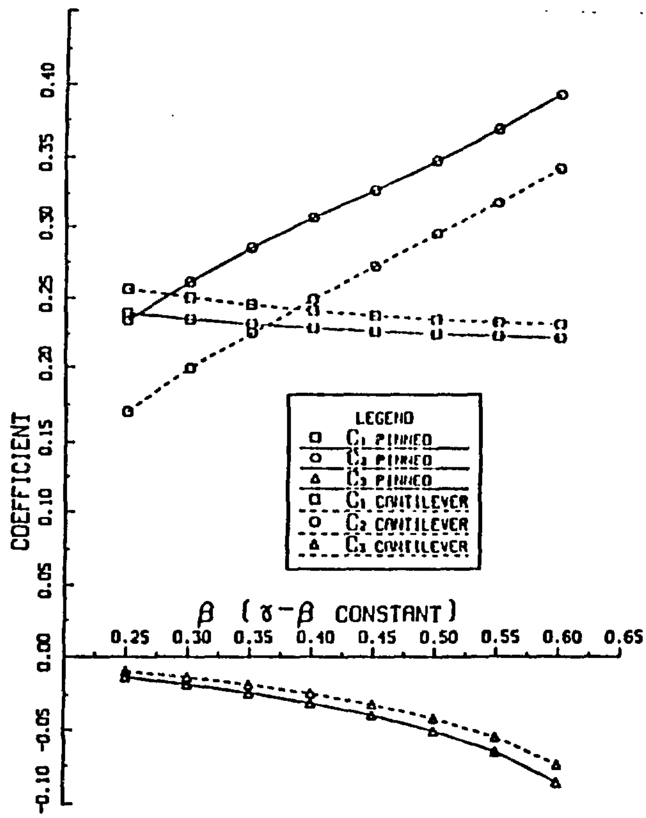

Fig 2. Temperature Dilation Coefficients as a Function of Core Axial Location, β

Location of the ACLP relative to the core, $\gamma-\beta$, is a less sensitive design parameter. Raising the ACLP can either modestly increase or decrease the fast coefficient, C_2, depending on the core location. It will also make C_3 more negative.

Load Pad Stiffness

The load pad stiffness, K, appears only in the temperature coefficients through Eq. (13) for the pinned support or Eqs. (15) and (16) for the cantilever case through the parameter $\lambda = EI/KL^3$. If K is sufficiently large, the influence of K is small. For example, if

$$K \gg \frac{3\,EI}{L^3 \gamma (1-\gamma)^2} \tag{21}$$

then ϕ in the case of pinned support is essentially independent of K and so also is the whole solution. Figure 3 shows the dependence of the temperature dilation coefficients on $1/\lambda$ which is proportional to K. 1/K represents the compliance of the ACLP plane interior to the core boundary driver assembly due to the bowing forces of the boundary driver and assemblies exterior to it. Since smaller values of K may lead to a negative value for C_2, and this implies decreasing radius with increases ΔT, inequality (21) can be considered as a design requirement for the above core load pad.

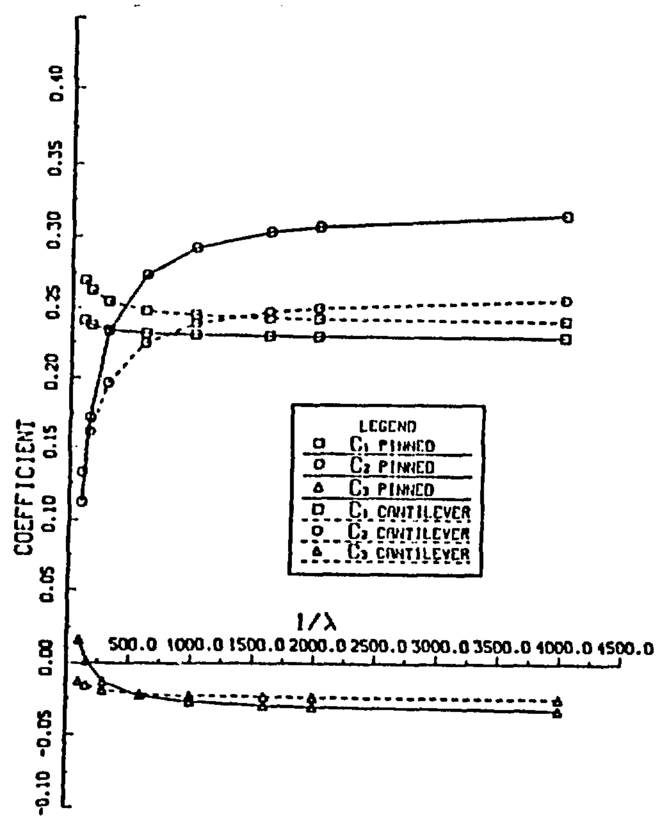

Fig. 3. Temperature Dilation Coefficients as a Function of Load Pad to Bending Stiffness Ratio, $1/\lambda$

Core Lock-Up

If we expand the core lock-up condition for the pinned case, set $\mu_3 = \mu_2$, that is the assembly and the restraint ring have the same thermal coefficient, and regroup the terms we have:

$$[\mu_2(\epsilon(\gamma)-\gamma\epsilon(1)) - (1-\mu_2)(1-\gamma)\rho_o]\tau_1$$

$$+ [\rho_o+\epsilon(\gamma)-\epsilon(\gamma)+\gamma\epsilon(1)]\tau_2 - \gamma(\rho_o+\epsilon(1))\tau_3 \qquad (22)$$

$$> \epsilon(1)-\epsilon(\gamma)-\eta(1)+\eta(\gamma).$$

Several observations can be made from this relationship. If the gap at the ACLP, $\epsilon(\gamma)$ is negative (usual) and $\mu_2<1$ then the inlet temperature lock-up coefficient, that is the term in (22) multiplying τ_1, is always negative. Increasing the inlet temperature tends to unlock the core. For an austenitic support plate and ferritic core materials the dominate term is the relative expansion of the grid plate over the core. If the materials are the same ($\mu_2=1$) then the phenomena can be controlled by the proper selection of the gaps, $\epsilon(\gamma)$ and $\epsilon(1)$.

The dominate term in causing lock-up is $\gamma\xi(1)\tau_2$. If we expand the expression for $\xi(1)$ using Eq. (1), then:

$$\gamma\xi(1)\tau_2 = \gamma[\frac{1}{2}(1-\beta)^2 + \frac{1}{6}\alpha^2]\tau_r\tau_2.$$

This term is dominated by $(1-\beta)^2$ so that lowering the core will facilitate lock-up as will raising the load pad, γ, and increasing the transverse thermal gradient, τ_r. For initially straight assemblies the thermal components of lock-up are balanced against the difference in gaps, $\epsilon(1)-\epsilon(\gamma)$. Later in life the inelastic bow terms, $\eta(1)$ and to a lesser extent $\eta(\gamma)$, will tend to control. Since lock-up of the core is really a core wide phenomena, the single assembly model does not provide a good quantitative measure by which to select design parameters. However, the qualitative insight into the effect of changing design parameters remains valid.

The Bowing Component

The model allows us to address the issue of the importance of bow relative to ACLP expansion in the *fast thermal response of the core. Creep and selling* bow only effect the lock-up state. Thermal bow effects both lock-up, as described above, and C_2 the fast temperature core dilation coefficient. For the pinned core the thermal bow term in C_2 is

$$-\frac{\beta\phi}{\gamma}\xi(\gamma) + \beta(\phi-1)\xi(1) + \xi(\beta).$$

In addition, even without thermal bow, the assembly must bow elastically to account for the interference of a thermally expanding ACLP relative to the nozzle and TLP supports. This term is:

$$\frac{\beta(\phi-1)}{\gamma}\rho_o.$$

Figure 4 shows the percent of the total fast coefficient, C_2, due to thermal bow alone and the percent of total bow (i.e. the thermal bow plus the additional elastic bow) as a function of location of the core within the assembly, β. For the pinned core

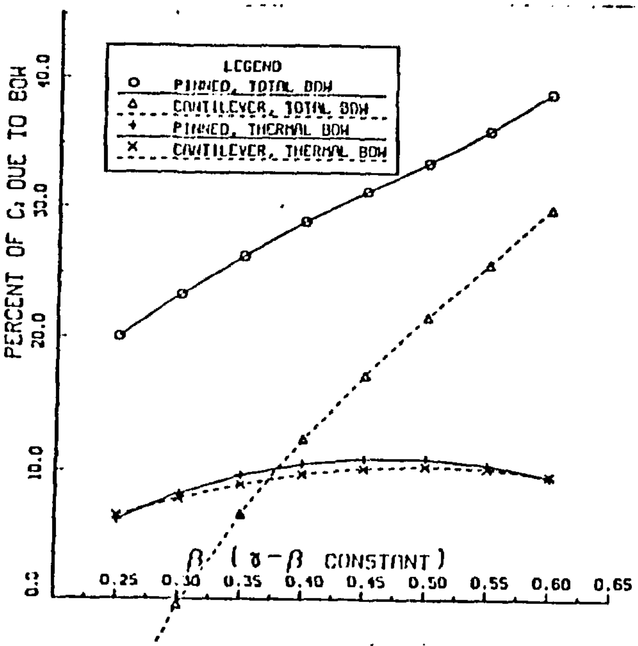

Fig. 4. Percent of Fast Dilation Due to Bow as a Function of Core Axial Location, β

total bow represents 30-50% of the fast thermal dilation coefficient, becoming more significant as the core height is increased. With a cantilever nozzle support total bow is a lesser contributor to C_2, particularly when the core is located low in the assembly. For the core located below 0.3 bowing decreases the magnitude of the fast coefficient.

Design Transient Calculations

The relative importance of the inlet temperature dilation coefficient, C_1, the fast coefficient, C_2, and the restraint ring coefficient, C_3, will in general depend on other plant characteristics. If, for a given plant, the critical transient and the critical times during those transients are known, this model will give guidance on how the design might be modified to increase inherent safety margins. It may be prudent, for example, to lower the core somewhat giving a larger value for C_1 at the expense of a smaller value of C_2 if the critical time in the critical transient occurs when the grid plate is heating up. Or it may be that ΔT is decreasing at the critical time so that a smaller value of C_2 is beneficial.

VALIDATION

This single assembly model of core thermal dilation has been validated by comparison with detailed calculations of assembly deformations for two core designs using the NUBOW-3D code. Two types of comparison were made. The first, termed an "integral" comparison, is made by comparing the sum of the reactivity changes due to assembly motions for all the individual assemblies of the NUBOW *simulation* with the reactivity change predicted by multiplying the core dilation produced by the single assembly model by the uniform dilation worth of the full core. Table C presents the results of these comparisons for a small LMR (425 MWt), a medium LMR (900 MWt) and for parameter variations on the small core.

The second method of validation is to formulate a vector version of the single assembly model, where both

Table C. Model Validation: Comparison with Detailed 3-D Calculations

Reactivity Change between P/F=1 and P/F=2

	SAM	NUBOW-3D
Medium Core	-22.4¢	-18.8¢
Small Core	-25.6¢	-28.1 to -29.7¢

Percent Reactivity Change between P/F=1 and P/F=2 due to change in:

	SAM	NUBOW-3D
Core Location	-21%	-20%
Load Pad Stiffness	-50%	-47%
Duct Material	+30%	+35%

the radial and circumferential components of the thermal and elastic bow are included, compare the resulting predicted displacement at the core midplane with the NUBOW calculated displacements. Figure 5 gives the results of this comparison for a P/F change of 1 to 2 at EOEC conditions for the small core. Similar results were obtained for other temperature fields.

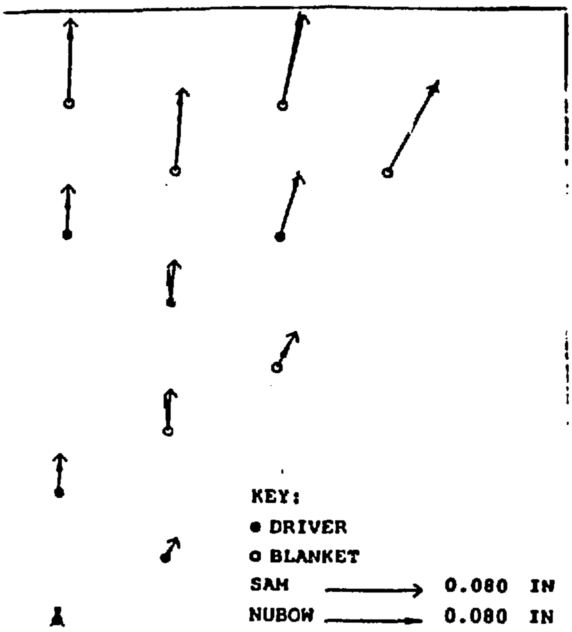

Fig. 5. Comparison of Midcore Assembly Displacements for a Small Core

CONCLUSIONS

We have developed an analytical model of core radial expansion for LFB designs at large P/F ratios. This model has allowed us to examine the effects of a variety of design parameters on core dilation. While conclusions as to the desirability of particular design choices depend on other design constraints we found that in general a more flexible nozzle support and a sufficiently stiff ACLP load pad enhance core radial expansion. Raising the core location within the assembly will enhance early core expansion due to rising core ΔT but will decrease long term expansion due to rising inlet and restraint ring temperatures.

This analytical model allows an analysis of the uncertainty of core radial expansion calculations which will be explored in a companion paper (4). That paper will also explore the affects on changes and uncertainties in core expansion on the inherent response of a reactor to beyond-design-basis transients.

The model is also useful for exploring the interaction between detailed assembly-displacement reactivity worths and expected assembly displacements during transients. A paper exploring this aspect of the inherent response of LMR's is in preparation.

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