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CORE RADIAL CONSTRAINT THEORY
AND APPLICATIONS TO THE FTR

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CORE RADIAL CONSTRAINT THEORY AND APPLICATIONS TO THE FTR

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Marlyn T. Jakub and W. Howard Sutherland

ABSTRACT

This document describes core restraint work completed for conceptual design of the Fast Test Reactor (FTR) during 1967. The design is based primarily on the dispersed conical core concept but is equally applicable to other core arrangements. The effect of radiation-induced stainless steel swelling was unknown during this period. The document is therefore concerned only with thermal bowing and its effects. The document is being released at this time to provide an historical background for later work on core restraint. The principles covered are still applicable and relevant to work on contemporary LMFBR restraint systems including the FTR.

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CORE RADIAL CONSTRAINT THEORY AND APPLICATIONS TO THE FTR

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INTRODUCTION

The first section of this report describes the various fuel motions in a fast reactor core and their dependence on flux, power generation, fuel material, fuel density, structural shapes, coolant, coolant mixing and coolant velocity. The second section pertains to lateral restraint methods for fuel ducts, with emphasis on EBR-II and Fermi designs. The final section reviews assumptions and results for conceptual radial constraint system models considered for the Fast Test Reactor.

Structural assembly of a fast reactor core such as the Fast Test Reactor (FTR) is considered to be a complex problem but nonetheless subject to analytical treatment and understanding. The complexity arises from the close thermal, mechanical, and nuclear coupling that exists in the core structure. Because the fuel in a fast reactor core is highly consolidated, small motions in core components can result in relatively large reactivity changes. Meltdown of the EBR-I core was attributed to nuclear-structural interaction,^{1*} and recent decreases in the power coefficient of EBR-II are also attributable to this cause. Designers of all subsequent reactors, particularly fast reactors, thoroughly analyze structural members which intertie with fuel movement. Such analyses take account of thermal gradients that introduce differential expansions in core assemblies and cause the core to either disperse or consolidate, depending upon the manner in which core assemblies are supported and restrained.

Radial constraint problems of the FTR are fundamentally no different from other fast reactors that are being proposed for the Liquid Metal Fast Breeder Reactor Program. The FTR, in common with LMFBR types, uses sodium coolant and has fuel

**Superscript numbers refer to the Reference Section at the end of the report.*

arrayed in cylindrical rods held in essentially vertical positions. The precise manner in which core restraint and support is accomplished of course differs, depending upon preferences and concepts. Analytical principles presented in this report represent the basis for efforts to ensure structural stability in the FTR core and should be of interest to all fast reactor core designers.

SUMMARY AND CONCLUSIONS

Differential thermal expansion causes various fuel motions in a fast reactor core that give rise to reactivity changes. The relationship between fuel motion and flux, power generation, fuel material, fuel density, neutron absorbers and moderators, structural shapes of cladding and duct, type of coolant, coolant mixing, and coolant velocity were investigated. Several methods for alleviating fuel thermal camber and hence reactivity changes, were evaluated to provide qualitative guidance. Significant conclusions obtained were:

- The response time for thermal expansion effects of duct supports is not fast enough to be in the same time response class as duct thermal camber if the supports are much thicker than the ducts.
- Lateral bending is the only significant reactivity feedback mode for cylindrical oxide fuel pins.
- With ducts and pin bundles, the principle feedback method depends only on duct lateral motion, not on pin or pin bundle motion within the duct.
- The differential equation governing stress-free lateral thermal camber of the duct depends only on the local coefficient of linear thermal expansion and on the mean slope of the transverse temperature profile. Duct stiffness and diameter are not involved.
- Maximum duct bending stress (along the axis) depends on local bending moment and duct diameter.

- Decreasing the slope of the flux profile will decrease the thermal camber deflections.
- Inlet orificing of subassemblies to match subassembly coolant AT in the core and varying fissile density with core radius are not effective methods for reducing thermal camber deflections.
- Coolant mixing generally causes little reduction of thermal camber deflections in ducts containing 100 or more pins and having coolant velocities of more than 10 ft/sec.
- Typical fast reactor radial constraint systems give transient reactivity feedback which depends only on flux, radial flux gradient, fuel density, and coolant AT.

The theory of duct radial constraint was applied to systems where reactivity increase can come only from inward duct motion. Simple systems were catalogued and an evaluation of the EBR-II, Fermi, and PFR reactors was completed. Preloading, duct creep deflections and support deflection were carefully reviewed for their effect on structural reactivity feedback. It was concluded from this work that:

- Preloading the ducts can minimize the duct stresses in high temperature operation; bending stiffness of the duct can be varied axially to give near-zero stresses under thermal camber loadings.
- Both EBR-II and Fermi restrain the thermal camber deflections at full power operation. This leads to having highest duct stresses at high temperature. Any small creep deformations will result in bowed ducts and a loose core during subsequent low power operations. Any gross collapse of central ducts may lead to radially inward fuel movement since the ducts are effectively spring-loaded to move inward.

Assumptions made for a reference FTR radial constraint system were formulated and various analytical models were

examined, including problems, design changes, and proposed tests. It was then concluded that:

- The current FIR approach to radial constraint is applicable to other FIR conceptual core designs as well as to other fast reactors.
- Each FIR duct is nearly stress-free or is spring-loaded outward.. Collapse of the central duct does not lead to positive reactivity feedback from other ducts.
- The current conceptual FIR design minimizes stresses at high temperature by using an inward preload of about 0.3 in. on the outer zone fuel ducts.
- In the outer fuel zone, average subassembly radial deflection (zero to full power) is about 30 mils outward. Reactivity temperature coefficient is a constant and is about $-0.0003\% \delta k / ^\circ F$ (coolant AT) for both orificed and uniform flow cores. These figures exclude thermal expansion of the core and core supports which adds a much larger negative reactivity effect.

I. RELEVANT PHENOMENA

A. NUCLEAR-STRUCTURAL STABILITY

Nuclear-structural interaction in a reactor core is a typical "transfer-function-and-feedback" process. When a nuclear power excursion occurs, there is an instantaneous generation of heat in the core fuel material. The excess heat then flows outward to the coolant, which heats the structural supports and causes thermal expansion deformations. Positive feedback arises from structural motion that moves fuel to a position of higher reactivity worth (generally toward the center of the core). The resulting increase in reactivity causes a rise in power, leading to greater motion and perhaps to an unstable system. A direct analogy is the idealized vibrating mass system under the influence of an external forcing function. A continued net energy input at the natural frequency of oscillation can lead

to greater and greater amplitudes until the system destroys itself. In a reactor, such an unstable power transient terminates with core meltdown or with a rapid core expansion caused by vaporized coolant or vaporized fuel.

Past work^(2,3,4) has shown that the nuclear-structural interactions will be stable and the power amplitude damped if:

- A significant fast-acting (<1 sec) and negative feedback mechanism exists.
- Any existing positive fast-acting mechanisms are offset by larger negative feedbacks with nearly the same response times.
- Any existing slow (i.e., many seconds or minutes) positive feedbacks are offset by larger negative feedbacks which have about the same or shorter response times.

Each response time regime can be analyzed independently if the goal is to demonstrate that within each regime no net positive reactivity feedback can occur from a reactor heating transient.

B. REACTIVITY FEEDBACK FROM STRUCTURAL DEFORMATION

A change in reactivity occurs whenever the following ratio changes:

$$k \equiv \frac{\text{Production rate of neutrons}}{\text{Loss rate of neutrons}}$$

For an entire reactor core, the above reactivity must be evaluated over all neutron energy groups ($i = 1$ to N) and over the entire volume ($\int dv$) of the core. By using perturbation theory on a multigroup diffusion model of the neutron distribution, (4a) a small reactivity change can be expressed as:

$$\begin{aligned}
\frac{\delta k}{k} = & \frac{K}{I} \int \left(\sum_{i=1}^N \left\{ C_i \delta \phi_i + \underbrace{\frac{1}{k} \delta \left(\nu \sum_f \right)_i \phi_i(r) \sum_{j=1}^N X_j \phi_j^+(r)}_{\text{& (fuel source of neutrons)}} \right. \right. \\
& - \delta \left(\sum_a \right)_i \phi_i(r) \phi_i^+(r) \left. \leftarrow \text{(absorber effectiveness)} \right. \\
& - \delta D_i \left[\overrightarrow{\Delta \phi_i(r)} - \overrightarrow{\Delta \phi_i^+(r)} \right] \leftarrow \text{(leakage)} \\
& \left. + \underbrace{\sum_{j=i+1}^N \delta \Sigma(i \rightarrow j) \phi_i(r) \left[\phi_j^+(r) - \phi_i^+(r) \right]}_{\delta \text{ (moderation effect)}} \right\} dv
\end{aligned}$$

$$\text{where } I = \int \left[\sum_{i=1}^N X_i \phi_i(r) \sum_{j=1}^N (\nu \Sigma_f)_j \phi_j(r) \right] dv$$

$$\begin{aligned}
C_i = & D_i \nabla^2 \phi_i^+(r) - \left(\sum_a \right)_i \phi_i^+(r) - \sum_{j=i+1}^N \Sigma(i \rightarrow j) \phi_i^+(r) \\
& + \sum_{j=i+1}^N \Sigma(i \rightarrow j) \phi_j^+(r) + \frac{(\nu \Sigma_f)_i}{k} \sum_{j=1}^N \left(X_j \phi_j^+ \right)
\end{aligned}$$

$\nu \Sigma_f$ = neutron production term from fission

Σ_a = neutron absorption cross section

The local change in flux level can be disregarded if the arbitrary weight-function, $\phi_i^+(r)$, is chosen in such a manner that C_i is zero. That is, the $\phi_i^+(r)$ is selected as the neutron worth or adjunct flux for energy group, i , at position, r . The leakage perturbation, δD_i , can also be disregarded for small power transients since the core surface remains essentially unchanged.

Reactivity change thus involves only perturbations in fuel location, moderation behavior, and absorber location. With control rods fixed, the only significant motion is caused by the fuel subassemblies. The dependence of $\frac{\delta k}{k}$ on fuel position will be discussed further in Section J. Since $\phi_1^+(r)$ goes to zero at the edge of the core, it is clear that reactivity change depends only on fuel subassembly positions. Any change in position toward the outside of the core will decrease the reactivity.

C. FUEL AND FUEL STRUCTURE CONSIDERATIONS

Large fast reactors generally involve designs with the fuel material carefully separated from the coolant by a metallic barrier or cladding. Cladding is subjected to high temperatures and temperature gradients while it supports the fuel structurally. The combination of requirements has been best achieved by using fuel containers of elementary structures -- long, thin plates or long, cylindrical tubes. To be consistent with previous fast reactor designs, tubular shapes are assumed for fuel containers in the present analysis. The axial direction in the core coincides with the long dimension of the fuel pins.

Consider now whether axial fuel movement is possible with the fuel enclosed in long pins. If heat is added to the fuel as in a nuclear excursion, then thermal expansion can cause net axial fuel movement if:

- The fuel column is not restrained by the still unheated cladding.
- There are no axial gaps in the fuel column.
- The fuel must behave more like a solid than a fluid.
- The hot fuel is not in a plastic state where thermal expansion is translated mainly into an internal pressure loading on the cladding.

Metallic or cermet fuels should exhibit axial expansion if fuel temperatures remain suitably low for the strength of the material. However, oxide and mixed-oxide fuels (such as in the FTR) generally fail all three conditions listed above.

Axial motion of an oxide fueled element will cause a reactivity change when the cladding heats up. However, the fuel must be held by the cladding (i.e., no slipping) and the motion will not be as great as the fuel temperature change would indicate. For example, in a sodium cooled reactor, the cladding temperature tends to follow the coolant temperature more than the oxide fuel temperature because of lower heat transfer resistance between the cladding and coolant than between fuel and cladding. A transient which raises average fuel temperatures by 100 degrees may only change the cladding temperature by 15 degrees. Because the principal interest is in oxide driver fuel for initial FTR cores, axial expansion of the fuel-cladding system is considered a negligible, but negative, feedback phenomenon. An evaluation of axial expansion effects in the FTR as an example of the above discussion is given in Appendix E.

Radial growth of fuel pins can be caused by cladding swelling, fission gas pressure, fuel swelling, or thermal expansion. Under the transients of interest, only gas pressure and thermal expansion can occur rapidly enough to be of concern. If the cladding is much stronger than the cracked oxide fuel and sustains only small transverse temperature changes, pin radial growth from heating will be disregarded. If the fuel is stronger in compression than is the cladding, then the cladding will yield during the transient. Radial growth effects of each pin are disregarded in the balance of this investigation. Although growth would not cause a direct fuel movement reactivity feedback of significance, the reduction of core sodium volume would change the reactivity slightly.

The only significant motion that remains is radial structural deformation of the fuel members. Radial deformation requires a forcing function that can arise from temperature gradients, external system vibrations, fluid-induced vibrations, or motions of supports. In this report only a long slender structural member deflected by temperature profiles is considered in the analysis.

D. RADIAL MOTION BY THERMAL CAMBER

The fuel pin is a long slender piece of structure which behaves like a beam under normal conditions. With highly turbulent flow and narrow flow paths, coolant temperatures can still vary from one side of a pin to the other. Variations are caused by flow maldistribution, uneven heat generation, uneven heat fluxes from cladding fuel gaps, steep flux gradients, and uneven pin separation distances.

Another beam element usually appears in the fuel support structure. The cluster of fuel pins comprising the subassembly is placed within a duct (also called a wrapper can) to:

- Provide a convenient unit for fuel handling operations.
- Keep coolant from bypassing the high flow resistance of the pin cluster.
- Provide a barrier against local accident propagation.
- Provide a structural matrix throughout the core which gives axial and radial support of the fuel pins.

The temperature around the edge of the duct will vary for the same reasons that are listed above for the pin temperature variation. Figure 1 illustrates that coolant temperatures can vary by 100 °F across a given duct. At the same time, the temperature variation can be as high as 80 °F across a single fuel pin within the duct.

For slight simplification in the analysis, it is assumed that the temperature variation depends on radial position in

FIGURE 1. Subassembly Radial Temperature Profiles

the core, and not on azimuthal position. The problem can be formulated using standard simple elastic beam theory with two additional assumptions:

- (a) Temperature profiles are either constant or linear (or approximately so) in the axial direction, and
- (b) The deflections and temperatures are within the elastic range of the material.

Appendix A gives the derivation of the following element equation for the lateral deflection, V :

$$\frac{d^2 V}{dx^2} = -b\alpha + \frac{\alpha}{I_z} \iint f_{\text{ODD}} y dy dz \quad (3)$$

where the temperature distribution (axis at center of beam) is put in the general form

$$T(y) = T_o + by + f_{\text{ODD}}(y) + f_{\text{EVEN}}(y) \quad (4)$$

and

I_z = moment of inertia at axial position x
 a = coefficient of linear thermal expansion at axial position x .

T_o = average temperature in the beam

b = slope of linear approximation of temperature profile at axial position x

$f_{\text{ODD}}(y)$, $f_{\text{EVEN}}(y)$ = odd and even functions of y .

The axial stresses in the unrestrained beam are given for position x , y , as:

$$\sigma_{xx} = -\alpha E \left\{ f_{\text{EVEN}} - \frac{1}{A} \iint f_{\text{EVEN}} dy dz + f_{\text{ODD}} \frac{-y}{I_z} \iint f_{\text{ODD}} y dy dz \right\} \quad (5)$$

For a fast reactor, the flux variation (or power generation) is nearly linear across the radial distance of one sub-assembly. Since this is the principal influence on the local coolant temperatures and hence, the local duct temperatures, a linear duct temperature profile may be assumed with good accuracy. This implies that the bracket term of equation (5) is zero and the duct is stress-free except for restraining loads. The deflection equation (3) becomes:

$$\frac{d^2v}{dx^2} = -b\alpha \quad (6)$$

Equation (6) can be superimposed with other beam loads to find the final deflection shape of the duct. It is noted that the thermal camber, in this simplified model, depends only on the coefficient of thermal expansion of the material (generally a constant) and on the slope of the temperature profile. This leads to the following generalization for a given beam undergoing thermal camber in a given structural support system.

The slope of the radial temperature profile determines the stress-free tube deflection shape; and the maximum stress is a linear function of the diameter at the point of maximum (axial) bending moment.

An alternative strength of materials derivation of equation (6) which starts from the basic assumption of a linear temperature profile is given in Appendix B.

E. RADIAL PIN MOTION INSIDE THE DUCT

Spacer grids or wire wraps are two commonly used devices which keep fuel pins separated inside the duct. With spacer grids, (Figure 2b) structural pieces resembling small honeycomb separators are located every few inches along the axis of the pin bundle. Each honeycomb structure separates the pins by means of lateral supports which have compliance (i.e., the contact point will yield like a spring as the compression load

increases). Significant motion of pins with respect to the support spacer grids can occur only if there is a weak spring constant in the support grid, i.e., if the fuel pin cladding is locally deformed at the contact points.

With wire wrap (Figure 2a), one wire is spiralled, at a specified pitch, around each fuel pin. If only every seventh pin were wrapped, then support points would occur six times, at every 60 degree rotation, for each pitch distance along the pin. If every pin in the bundle is wire wrapped, then support points occur 12 times for every pitch distance along the pin. Around the circumference of the pin, up to three support points may contribute components in a given direction. Thus the effective distance between supports on a simple beam model of the pin would be approximately one-third the pitch distance. Unlike spacer grids, the wire wrap has little or no compliance and, generally, a diametral gap is added to keep the bundle from binding. On an FIR bundle of 4.5 in. flat-to-flat hexagonal pattern, 0.25-in. diameter pins and 0.030-in. wire wrap, the needed gap to allow for tolerances can be restricted to less than 20 mils.

With spacer grid elements, the additional room for pin swelling can be accommodated by the compliance of the support points. With small pin diameters (0.25-in. OD), the flexibility of fuel pins will mean that only small forces at the contact pads will be able to restrain the pin thermal camber. The compliant nature of the grid support will only be utilized by the large forces associated with swelling.

Much work^(5,6) has been done for a beam model of the fuel pin using fixed support points equally spaced along the length of the pin. Appendix C illustrates how the FIR pin design might deflect. Figures 3 and 4 show a typical pin model and how the deflection curve is sinusoidal for normal temperature patterns. Net lateral deflections of the pin when averaged

a. Fuel Pin with Wire Wrap

b. Fuel Pin with Spacer Grids

FIGURE 2. Fuel Pin Spacing Methods

FIGURE 3. Geometry and Temperature Distribution for the Multip Support Fuel Pin

FIGURE 4. Radial Displacement and Bending Moment in Pin

over the core length, are small fractions of mils. Under abnormal temperature patterns (e.g., flow blockage), the lateral deflections can be larger.⁽⁷⁾ In Appendix C local deflections of several mils are calculated for a simple pin model.

For the remainder of the report, the reactivity feedback from pin motion relative to spacers and other pins (in the same bundle) will be disregarded. This could prove to be a poor assumption for wire wrap bundles with large diametral gaps, but with spacer grids, the fuel bundle now deflects as a unit. There may be internal motion and stresses in the bundle, but, for reactivity effects, the mean temperature gradient determines the thermal camber of the entire bundle. The following is an evaluation of how this bundle can move laterally within the duct.

Diametral clearances are left around the pin bundle for ease of assembly and to allow room for thermal growth and irradiation swelling of the pins. Because the pin bundle mean temperature gradient may not precisely match the duct temperature gradient (see Figure 1) the cases shown in Figure 5 can arise.

The mean diametral gap is usually small. For the FTR, the 4.5-in. subassembly has been using ~30 mils. The net (axially averaged) lateral movement of the fuel is a few mils less than cases a and b of Figure 5. In Figure 5c, the temperature gradients are greatly mismatched, and the net movement may be 10 to 15 mils. These various cases are defined by preloads and the slow rise from zero to full power (Figure 5). For the transient analysis of interest in this report, (i.e., a perturbation in power from an initial steady-state high power level) the net lateral motion of any given fuel subassembly will be on the order of a few mils. When many subassemblies over the entire core are considered, the random nature of the early portion of the curve in Figure 6 and the variety of positive and negative feedbacks (Figures 5b, 5c) lead to the following assumption:

FIGURE 5. *Pin Bundle Motion in a Duct*

FIGURE 6. *Assumed Reactivity from Fuel Pin Bundle Motion in a Rigid Duct*

The small fuel pin motions within the bundles and the small fuel pin bundle motions within the ducts can cause significant positive or negative reactivity effects within a given subassembly, but the overall reactivity effect of these motions is negligible when one considers the entire core.

The validity of the above assumption is certainly true for transients in which the change (in mean thermal gradients) occurs simultaneously and proportionally in duct and pin bundle. No change occurs in positions. For spacer grid systems, the possibility of cancelling feedbacks as shown on Figures 6b and 6c means that the reactivity effects may indeed be "negligible" instead of "small." For wire-wrapped systems, the assumption should be evaluated by experiment.

F. RELATIVE RIGIDITY OF COMPONENTS

The fuel pins and duct will now be considered as one structural system. If the duct thermal camber does not match the thermal camber of the pin bundle, then which one dominates? Again, the detail design must be investigated.

If the pins are supported so that they can slide in the spacer grids and support grids, then the pin bundle can bend much like a deck of cards. Only small friction shear loads are transmitted to adjacent pins. Disregarding friction, the rigidity of the pin bundle is just the rigidity of an individual pin times the number of pins. For a round, thin-walled tube, the rigidity can be approximated as follows:

$$I_z = \text{constant} \quad (D - d)^4 = KD^4 \left[1 - \left(\frac{d}{D} \right)^4 \right] \approx KD^4 \left(4 \frac{t}{D} \right) = 8 KD^3 t \quad (7)$$

where

D = outside diameter
d = inside diameter
t = thickness

Suppose the duct thickness to pin-cladding thickness ratio is 8, and the duct diameter-to-pin diameter ratio is 10; then, if 200 pins are in a bundle, a typical comparison of rigidities gives:

$$\frac{I_{\text{bundle}}}{I_{\text{tube}}} = \left(\frac{1}{8}\right) \cdot \left(\frac{1}{10}\right)^3 \cdot 200 = 0.025 \quad (8)$$

The duct will dominate and force the pin bundle to conform to the duct deformation profile.

With friction at the spacers between spacer and duct, or with welded spacers, the answer is not as obvious and requires detailed analysis or testing. Even if the coefficient of friction is low, the normal loads can be increased by fuel swelling. Finally, self-welding may occur. Any combination of these effects will effectively "glue" the elements together so that bending rigidity is increased. The maximum pin bundle rigidity can be compared to that of the duct:

$$\frac{I_{\text{bundle}}}{I_{\text{tube}}} = \frac{KD^4 \cdot (\text{density ratio})}{8D^3 tK} \quad (9)$$

The density ratio is simply the cross sectional area of the pin cladding compared to the total area enclosed by the duct.

$$\text{density ratio} = \frac{200 \delta (\pi h)}{\frac{\pi D^2}{4}} = \frac{800 \delta h}{D^2}$$

where

δ = cladding thickness

h = pin diameter

D = duct mean diameter

Thus, the rigidity ratio becomes

$$\frac{I_{\text{bundle}}}{I_{\text{tube}}} = \frac{800 \delta h}{8 D t} = 100 \left(\frac{\delta}{t}\right) \cdot \left(\frac{h}{D}\right) = 100 \cdot \frac{1}{8} \cdot \frac{1}{10} = 1.25 \quad (10)$$

and the duct no longer dominates the motion of the fuel.

G. COOLANT MIXING WITHIN THE BUNDLE

This part of the analysis deals with basic temperature patterns in the duct and how the power generation curve can be used to estimate subassembly duct temperatures throughout the core.

A subassembly is considered in which no mixing occurs, and fuel density and coolant flow are uniform across the bundle. If the profiles of the core radial and axial flux are separable and constant during the transient, then the radial temperature difference of the duct walls will develop in conjunction with the heating of the coolant.* At the core outlet, the radial temperature difference across the duct maximizes (see Figure 7) and is given by (see Appendix D for derivation):

$$\Delta T_r \Big|_{\text{outlet}} = 2 \left[\frac{\phi_{\text{peak}}}{\phi_{\text{avg}}} - 1 \right] \Delta T_{\text{axial}} \quad (11)$$

The flux ratio in the above equation is equivalent to the power generation ratio in the element. With a constant power generation gradient in the radial direction (across the tube), the quantity in the bracket represents one-half the total fractional change in the power generation across the duct. Power generation slope at a given axial position is then:

$$\frac{2 \left[\frac{\phi_{\text{peak}}}{\phi_{\text{avg}}} - 1 \right]}{\text{Duct Diameter}} = \text{radial gradient of power generation.}$$

**For high velocity coolant flow, the coolant temperature rise becomes essentially linear with distance traversed in the core region.*

FIGURE 7. Axial Temperature Gains for Coolant in Orificed and Uniform Flow Cores with Uniform Flow Density

Duct thermal camber is proportional to power generation slope if it is assumed that:

- Duct temperature is governed by coolant temperature adjacent to the inside surface.
- A constant peak-to-average power generation occurs along the fuel subassembly length.

A radial temperature difference proportional to the power generation profile exists across the duct (Figure 7).

Mixing was not prohibited by the above assumptions -- so a constant can be inserted into equation (11) and the above requirements will still be met.

$$\Delta T_r \Big|_{\text{outlet}} = 2 K_{\text{mix}} \left[\frac{\phi_{\text{peak}}}{\phi_{\text{avg}}} - 1 \right] \Delta T_{\text{axial}} \quad (12)$$

If coolant mixing is limited within a given subassembly, then K_{mix} will be close to unity. Fast reactors have tended to use small (~ 0.25 in. OD) pin sizes, with 100 or more pins inside each duct. High coolant velocities, closely-spaced pins, and short core lengths have made it impractical to achieve substantial coolant mixing within a subassembly, even with wire wraps or flow-distorting spacers. Temperature patterns within FTR are typical of fast reactor designs. The analysis for FTR temperature patterns is reviewed in Appendix D. In the typical fast reactor subassembly, coolant mixing can only be effective in a few edge channels next to the duct. The above equation is relevant for analyzing this type of subassembly.

H. FLOW ORIFICING

First, the case of no mixing and uniform fuel density is considered. With this assumption, the radial constraint problem is proportional to the slope of the flux curve (Figure 4) times the axial ΔT of the coolant in various subassemblies as in equation (11). The highest coolant axial temperature differences occur in the central portion of the core where the radial slope

of the flux is small. Thus, ΔT of equation (11) has a maximum corresponding to the worst bowing located somewhere away from the central core region, but not necessarily at the edge of the core.

An orificed core is now assumed where the pressure loss is adjusted at the subassembly inlet until the coolant flow matches the average power of the subassembly (see Figure 4). Each fuel subassembly flow is adjusted until the coolant bulk temperature rise across the core will be the same for all sub-assemblies, regardless of radial position. Unfortunately, sub-assemblies in the outer portion of the core will now have bowing problems because of the larger temperature gradients (see Figure 4). Thus the benefits of orificing must be evaluated against the more severe thermal camber of elements in the outer portion of the core.

1. MULTIPLE DENSITY POWER FLATTENING

If the fuel density in a given fuel pin could be varied over a wide range without much difficulty a core could have every coolant channel with uniform flow and a uniform radial temperature profile. The local power generation would precisely match the local flux, and thermal camber would be negligible. Practically, the fabrication of fuel requires that the fuel density be constant within a given pin. Even a radial variation of fuel density a few times over the core involves a more complicated fuel fabrication method, a more complicated fuel relocation program, and an inherent safety problem.

Figure 8 illustrates the power profile in a two-zone core where each half of the core subassemblies have different fuel densities. Because the power slope increases beyond the transition, the subassembly temperature differences increase for the outer portion of the core in a manner similar to orificed cores. If fuel density of a subassembly was determined by its

FIGURE 8. FTR Radial Power Profile Perpendicular to Loops

radial position, then, by proper fuel density selection, the uniform flow core could theoretically duplicate the curve (Figure 4) for the orificed core. Obviously, the thermal camber deflections would also be identical except for differences in the neutron flux distribution between one- and two-zone cores.

J. SIMPLE REACTIVITY RELATIONSHIPS

To determine reactivity changes in a core, the motion and reactivity worth of each fuel segment must be known. In general, the motion and reactivity worth are functions of radius, axial location, and possibly azimuthal position. However, if the assumptions discussed in the previous sections are made, several simple reactivity relationships can be developed for the reactor core. The assumptions are:

- Fuel moves only laterally.
- Fuel in a given subassembly is controlled by duct deformation.
- Ducts deform only from thermal camber.
- No thermal expansion of supports.
- The lateral deflection (as a function of axial position in a subassembly) has a shape that is independent of amplitude. That is, net lateral motion of a subassembly is proportional to the net deflection at any point in the core.

From Figures 9 and 10, it is also assumed that the fast reactor has a worth $(\frac{\delta k}{k_g})$ that is nearly represented by the square of the flux distribution.

$$R(r) = \text{reactivity worth at a given radius} = K_o \phi^2(r) = K_1 \left[\frac{P(r)}{\rho_F(r)} \right]^2 \quad (13)$$

FIGURE 9. ²³⁹Pu Worth Versus Axial Position in FTR (Ref BNWL-603)

FIGURE 10. Fuel Worth Normalized to Square of Flux in FTR
(Perpendicular to Loop Chaseway)

where power generation, P , is related to ϕ by the equation,

$$P = K\rho_F\phi.$$

The reactivity change per unit radial motion, $\frac{\Delta R}{\delta r}$, is then just

$$\frac{\Delta R(r)}{\delta r} = 2K_O \phi'(r) \phi(r) = 2 \frac{K_1}{\rho_F} P'(r) P(r) \quad (14)$$

where we have assumed that the fuel density, $\rho_F(r)$, undergoes only step changes and thus acts as a constant for the differentiation.

By neglecting subassembly coolant mixing, we obtain the thermal camber or radial movement of a duct as (from equation (12)),

$$\delta r = K_2 \left. T_r \right|_{\max} = K_3 P'(r) \Delta T_{\text{axial}} \quad (15)$$

where δr is measured from the position when the reactor is at zero power.

For coolant in an orificed core subassembly at radius, r ,

$$\Delta T_{\text{axial}} = K_4 \quad (16)$$

For coolant in an unorificed core subassembly at radius, r ,

$$\Delta T_{\text{axial}} = K_5 P(r) \quad (17)$$

Combining the above relationships gives the change of reactivity in the core (from zero power reactivity) as:

$$\Delta R(r) = \begin{cases} \frac{K}{2\rho_F} [P(r) - P'(r)]^2 = K \left[\frac{\partial}{\partial r} (\phi(r)) \right]^2 \phi(r) \cdot \rho_F & \text{Orificed Core} \\ \frac{K}{2\rho_F} [P(r) - P'(r)]^2 = K [\phi(r) \frac{\partial}{\partial r} (\phi(r))]^2 \cdot \rho_F^2 & \text{Nonorificed Core} \end{cases} \quad (18)$$

To find the net change in core reactivity from the zero power reactivity, the above expression must be evaluated over the core volume as follows.

$$\left[\begin{array}{c} \text{Change in Core} \\ \text{Reactivity} \end{array} \right] = \bar{K} \int_0^{\text{edge}} \Delta R(r) r \, dr \quad (20)$$

where $\bar{K} = 2\pi \cdot (\text{core height})$ for uniform distribution of fuel in a given core radial ring.*

The significance of $\Delta R(r)$ is that it states the relative reactivity change from the motion of a given fuel subassembly.

K. FLUX CONTROL

Statements expressed in previous subsections were based on the assumption that the neutron flux profile was relatively fixed. The neutron distribution in fast reactors can vary somewhat as shown by the power distributions in Figure 11. For a given flux magnitude, thermal neutrons will cause a higher power level because of higher fission cross-section. Thus the addition of a moderator (like BeO) into the core will usually cause a local power peak. Also, the addition of neutron-absorbing poisons to the core would reduce the flux locally.

*If there are open loop or closed loop regions, or other reasons for nonsymmetry in a given radial ring, then $\bar{K}$ may be modified in order to obtain a first estimate on core reactivity behavior.

FIGURE 11. *Special Power Profiles*

Implications of flux control methods to thermal camber are immediately seen by reviewing part J. above. Reactivity feedbacks may be entirely governed by the flux profile. Thus flux control represents the most powerful way to control thermal camber and structural bowing. Practically, the addition of the large amounts of BeO (see discussion of amounts in Reference 8) or poison is usually undesirable because of space limitations and neutron economy. Other methods for controlling thermal camber without changing the flux are discussed below.

L. OTHER SPECIAL SCHEMES TO CONTROL THERMAL CAMBER

If a desire exists to limit thermal camber in a core that has the usual cosine-shaped flux profile, the following list of ideas may be considered. In each case, the flux profile may change slightly, but none of the methods are aimed primarily at flux alteration. Each method is aimed at reducing temperature differences between various parts of the structural support.

1. Double Duct

The outside duct is the structural member and the inner duct simply keeps the fuel coolant away. Between the ducts, an insulating gas can be used so that the duct temperatures are governed by slow-moving coolant between the ducts. Alternatively the gap can be made into helical flow paths for the coolant. Fermi reactor personnel experimented with the latter approach and discarded it because of (a) high pressure drops (in the annulus), (b) the fact that the transverse gradient could never be completely removed;" and (c) because of success with single-duct designs.

2. Central Support Rods

Main support rods are placed near the center of the pin bundle and there are special coolant circulation paths to keep the temperature differences low. The placement in the center per se does not reduce bowing magnitude since the slope of the power generation profile controls this magnitude. However, the central placement may allow good mixing. Note that even a thin duct on the outside would easily dominate (see subsection F) the stiffness of the rods. Thus the design can be considered only for ductless cores.

3. Hollow Rods

A ductless core could use larger hollow rods for stiffness. The temperature would be controlled by orificed internal coolant flow. An external insulation coating might also be required in liquid metal systems since the conductivity losses could be large.

**Temperature gradients are particularly troublesome when the transient is faster than the half-cycle time for the coolant flowing around the duct.*

4. Varying Coefficient of Thermal Expansion

Make a duct tube with varying coefficients of thermal expansion. For example, a hex tube could consist of six slightly differing materials that are seam welded together. Making pins with assorted cladding coefficients of thermal expansion is an equivalent alternative if the duct is weak and pins are fastened to more than one support.

5. Varying Coolant Flow Paths

Using different sized flow paths that are larger toward the core center would eliminate temperature differences across the duct. The duct would have an unusual shape and the core fuel density would vary unless a special packing array can be envisioned.

6. Varying Coolant Flow Along Outside Wall

Place additional wires (axially) in the wall channels of a duct, but only on the side away from the core center. In the element shown in Figure 1, the radial temperature pattern in the entire bundle would be flattened by raising the outer wall temperatures. All other temperatures are slightly lower because of additional flow.

II. RADIAL CONSTRAINT SYSTEMS FOR DUCTS

A. END CONDITIONS

This section deals entirely with lateral constraint methods for ducts which can be analyzed as beams. The number of redundant lateral supports on the duct can be arbitrary, in theory, but most core constraint designs limit the number to three or four.

Linear end supports shown in Figure 12 are easiest to analyze. With a free end, rotation, deflection and expansion can occur. A pinned end allows only rotation, and a built-in end allows neither rotation nor deflection. The other end condition

FIGURE 12. *End Conditions*

examples in Figure 12 are simply variations on the three basic types. As long as axial support of the subassembly is provided without introduction of high buckling loads on the core, the two end conditions can be almost any combination of the types shown.

The lateral support, if one exists near the core elevation, generally falls into one of the categories of pinned, structurally pinned, preloaded and pinned, or free-pinned. Space limitations and high internal heating from irradiation require that contact pads, rings, or projections on the ducts form the middle support.

B. ELIMINATING CORE MIDDLE SUPPORTS

A simple radial constraint system is preferable if it ensures net outward fuel motion from a heating transient. Figure 13 shows several ways to eliminate the central support.

There may be other ways to accomplish the same objective, but the interplay of stiffness regions and deformation shapes should be evident from the examples given. When the beam is redundantly supported, stress levels must exist at either the hot condition or at the cold condition (e.g., a preloaded structure).

Figures 13a and 13b illustrate the only acceptable ways to achieve correct motion without core radial supports, while still maintaining zero stress in the duct at all times. A constant temperature gradient is assumed across the subassembly. Note also that a stress-free condition is guaranteed by either two simple supports or by one fixed support. Several stress-free cases which give negative reactivity feedback are not shown since they involve locating one of the supports at the core elevations. Figure 13a is the case to be avoided in designing the radial constraint system since it gives rise only to inward deflection.

FIGURE 13. *Constraint Without Middle Supports*

C. SIMPLE SYSTEMS WITH PINNED SUPPORTS

The true built-in end condition on a duct is difficult to achieve without welding. Tightly toleranced, slightly beveled surfaces are probably the best design. Reactors have tended to favor the moment-restrained type of end condition which uses two support points. Unfortunately both supports cannot be seated at the same duct position and there remains a small gap at the unseated contact plane. The way to simplify joints is to make all of them approach a simple (or pinned) support point as in Figures 12c, d, and e.

If an attempt is made to use only a minimum number of pinned joints, then systems similar to Figure 14 are possible choices. Figure 14a shows how the central core support is located where the transverse temperature gradients lead to a balanced outward deflection. Since the deflection is outward, the core location would be acceptable in any of the three locations shown. However, the core location and balanced central support are not independent since the transverse temperature gradients depend on core location, flow direction, and subassembly design. If the transverse temperature gradients extend over too much of the axial length of the beam, then the thermal camber deflections may exceed the deflections from a point load; and the final deflection is inward except at support points.

By varying the axial stiffness (e.g., changing diameters or wall thicknesses), the two cases 12b and 12c will apply, respectively, to upflow and downflow. Another feature of these two systems is the location of the central support away from the immediate core region. Internal heating, welds, stress concentration factors, and localized contact deformations often make an out-of-core location more desirable. The FTR system is much like Figure 14b and will be discussed in Section III Radial Constraint Analysis.

FIGURE 14. *Systems with only Pinned or Simple Supports*

D. MOMENT-RESTRAINED AT LOWER END

Finally, simple systems are considered which use a moment-restrained end condition at the lower end. This design type has been a favorite for most of the current fast reactors. The only really new method is shown in 13d where an added degree of redundancy is used. Core position 2 in Figure 15d is similar to the Fermi system which will be discussed later. Figure 15d is also similar to the EBR-II constraint system at full reactor power condition although gaps will appear at supports. Figure 13a is typical of part of the EBR-II system under low power operation when gaps occur at the top and middle supports.

E. PRELOADING

Preloading is accomplished by imposing a force (and deflection) on a duct support prior to reactor operation. The advantages are usually one or more of the following:

FIGURE 15. Moment-Restrained at Lower End

- Ducts in the core become rigid and no gaps remain which would allow arbitrary motion by any of the ducts.
- Cold stressing of ducts will allow the duct to be in a low stress condition when at full power operation.
- Low stress at high temperature eliminates warping of the duct from creep deformation. Straight tubes emerge after their life in the reactor.

The second advantage of preloading can be best explained with the use of Figure 16. Consider the duct thermal deflection curve for a redundantly-supported duct at full power conditions. If the preloaded support point is unloaded, the point will move until a minimum energy condition exists in the duct. If the support point were preloaded to this position when the reactor was cold, then the duct stresses would be highest when the reactor is at low temperature, and the stresses would be minimized at high temperature operation. Note that the preload for Fermi is opposite to the direction for reducing high temperature stresses. By careful design of the support locations and the tube structural stiffness, the deflection curve of the preloaded beam can approximate the stress-free curve at full power. The designer's objective is to make the point-loaded, cold beam result in a suitable shape (by careful selection of rigidities) that will match the stress-free, heated duct under the influence of thermal camber. As explained in Section I, the stress-free thermal camber position is independent of the stiffness of the duct, and the rigidity can be arbitrarily selected within a range of diameters and tube wall thicknesses.

F. HISTORICAL EXAMPLE - FERMI

Since the problem of core radial constraint has been faced in other fast reactor designs, it is of interest to review theory and practice as applied together to several of these. In the Fermi reactor a report was written by de Stordeur (Reference 10)

FIGURE 16. *Top Preload of Fermi Duct*

on the theoretical analyses and development experiments for the radial constraint system. That report was used as a guideline for the following description.

Fermi subassemblies contain metallic uranium fuel pins arranged in a square lattice inside a square duct (or wrapper can). The pins are separated by flexible spacer grids located approximately every two in. along the axial length of the pin bundle. Upward flowing sodium cools the fuel and duct. The system shown in Figure 17a was eventually used for the radial support of the ducts.

The analytical model for Fermi radial constraint used the following assumptions:

1. Negligible feedback from pin motion in the bundle
2. Negligible feedback from pin bundle motion in the duct
3. Pins free to expand independently -- no binding
4. Pin bundle moment of inertia is negligible vis-a-vis duct
5. Duct is elastic material at all times with constant material properties

FIGURE 17. *Fermi Models*

6. Flux and power generation are typical smooth cosine functions in the radial and axial directions
Duct temperature equals the temperature of adjacent internal coolant
8. Coolant temperature in the ducts causes the temperature gradient shown in Figure 17. Note that negligible mixing is assumed for coolant in the blanket region above the core
9. Ducts are zero stress in vertical position with reactor shut down
10. Pads, supports and holddown grid at top are either rigid or stiff springs
11. Negligible axial loading on ducts; thus, no buckling occurs. Superposition is valid
12. Thermal expansion of midplane supports and top holddown grid were included

The following comments on assumptions 5, 8, 9, 10, and 12 are offered:

- By assuming that no mixing occurs above the core, the thermal camber loads (and therefore the stresses) will be maximized for the duct. The deflection pattern will change if a linear decay in ΔT_r occurs above the core, and the new deflection pattern will have less negative reactivity feedback. Some safety analyses require minimum possible negative reactivity feedback which would require a different temperature profile assumption.
- Because the highest stresses occur at highest temperature, creep deformation is more likely to occur. Actual operation has not resulted in bent ducts, but during a bad transient, the higher duct temperatures often will lead to inelastic deformations." Furthermore, if the tubes become deformed, the zero stress condition is not a true vertical profile and there is no longer assurance that the ducts are in compression (at the core support pads) at startup. This point is discussed later in Section J.
- The thermal expansion of the ducts should be included for the core support since no special thick pads were used to slow the response time. However, the holddown grid has a built-in delay time from its massiveness and the time it takes outlet coolant to reach the support ($\sim 1/2$ sec). Thus the thermal expansion of the top support should not be included in fast transients. The duct will have slightly higher stresses but will cause roughly the same reactivity behavior during the fast transient.

The top holddown piece preloads the top of the duct inward toward the center of the core. As shown on Figure 15,

**Bent ducts were removed from subassemblies near the two which melted in the October, 1966 meltdown incident.*

the removal of the top restraint would ostensibly make a stress-free duct. With the preload direction now used the preload deflection is contributing additional stress into the duct.

The goal of the preload was to ensure compressive loads in all ducts at the core support pads. However, section J that follows will show that even this goal cannot be ensured if creep deformation ever occurs within the tubes. Even if compressive loads are achieved in the core support plane, there is an undesirable feature. If any central tube collapses (say, from melt-down), the compressive loads are relieved by outer ducts (same row as collapsed duct) moving inward and causing a positive reactivity feedback. Sufficient rubbing friction and interlocking ducts are two ways of preventing this motion.

Other constraint models were analyzed for Fermi. The one shown in Figure 17b was rejected. However, the top-of-core location for pads did result in better negative reactivity feedback but with higher duct stress. A double-walled tube (as in Section I-L) was tried but rejected. The disadvantages of this type of system include:

- Fabrication costs are high for double duct
- All radial temperature variation cannot be eliminated in the duct
- Any transient causes a quick rise in ΔT_r which cannot be quickly damped out
- Pressure drop requirements for adequate coolant are too high (in the annular region)
- Flow in the annulus is bypassed and causes unusual temperature patterns near the duct walls and at the duct outlets
- A significant decrease in fuel density is required for the extra duct wall and coolant annulus.

Another rejected method used a special holddown head for the top of the duct. The idea was simply to allow the duct to move laterally, thus reducing stress levels. Unfortunately,

experiments indicated that rolling friction was significant and unpredictable. In addition, the earliest motion would produce positive reactivity feedback until the pads contacted adjacent ducts. A summary of feedback coefficients for Fermi is shown in Table I.⁽¹¹⁾

TABLE I. *Temperature Coefficients of Reactivity for EBR-II and Fermi*

<u>Mechanism</u>	<u>EBR-II $\delta K/K$ per Core AT, ($\times 10^{-6}/^{\circ}\text{F}$)</u>	<u>Fermi $\delta K/K$ per Core AT, ($\times 10^{-6}/^{\circ}\text{F}$)</u>
● Axial Fuel Expansion	-2.2	-1.4
● Sodium Expulsion from radial expansion of pins	-0.5	-0.3
● Density Changes of Coolant and Support Structure	-5.3	-4.0
● Structural Expansion (including thermal camber)	-5.4	-3.3

G. HISTORICAL EXAMPLE - EBR-II

The final EBR-II radial constraint system appears in Figure 17. The gaps are the unusual feature in this design. At the bottom, the lowest tube sheet has some clearance before it can change the end condition from pinned to moment-restrained. The gaps at the core support plane and at the top of the duct are caused by adjacent subassemblies. Obviously, the entire core must be deflection-analyzed to achieve compatible duct deflection for the entire core. Otherwise the gaps for a given element will not be precisely known. An early analysis of EBR-II bowing was considered by J. C. Carter and H. O. Monson (see Reference 12) using two possible beam support configurations for each duct. A computer program has been used for more refined (five beam configuration) analytical models of EBR-II.^(13,14) The latter program analyzes the entire core for compatible deflections.

The latest EBR-II models use the following assumptions:

1. Fuel pins (wire wrapped in a hexagonal pattern) have negligible feedback from motion inside pin bundle
2. Negligible feedback occurs from pin bundle motion inside hex duct
3. Individual pins are free to expand axially without binding
4. Pin bundle moment of inertia is much smaller than that of duct
5. Duct is elastic material with constant material properties during transients
6. Flux and power generation are typical smooth cosine functions in the radial and axial directions
7. Duct temperature is governed by temperature of adjacent internal coolant
8. Coolant temperature variation causes the radial temperature gradient of Figure 18. Note that coolant mixing is assumed to occur in the gas plenum region, leading to a linear reduction of transverse temperature gradients
9. Ducts have zero stresses when in vertical position with the reactor down
10. Contact dimples act as stiff springs, tubesheet supports are rigid, adjacent ducts are flexible in bending
11. Negligible axial loading exists in the ducts. Thus, no buckling occurs and superposition is valid
12. Thermal expansion of the ducts is incorporated for the core support at the dimples and at the top of the duct.

The following comments are relevant to assumptions 5, 8, 9, 10, and 12:

- As in Fermi, EBR-II ducts have highest stress during operation and any heating accident may lead to deformed ducts.
- Some deflection has been noticed near the pads⁽¹⁴⁾ and this deflection is believed to be the cause of the changing "reactivity-feedback-versus-power-level" curve. See Subsection I for further discussion.

FIGURE 18. *EBR-II Radial Constraint Model*

- For fast transients, the transport time lag ($\sim 1/2$ sec) from coolant transport from core to the top subassembly support pads implies that the duct acts independently of thermal expansion effects at the top of the duct. This will probably mean a reduced negative reactivity feedback. The thermal expansion at the midcore support should be retained in the fast transient model because thin dimples have the same time response as thermal camber.

With five possible beam support modes, the duct motion may be complicated. However, the sequence usually occurs as follows:

- The initial motion is likened to Figure 15a until the outermost core duct row is stopped by the top pads of adjacent subassemblies.

- Inward fuel motion occurs until the core support dimples interact with enough force to halt the inward duct movement.
- If gaps between dimples are too large (2 mils or greater), the core may never achieve the tight compaction. The thermal camber by itself causes slightly positive reactivity in going from zero to full power.⁽¹⁴⁾ However, structural thermal expansion gives a net negative reactivity feedback when proceeding from zero to full reactor power (see Table I).

Recently,⁽¹³⁾ the radial rows 6 and 7 of the EBR-II core have been converted from blanket elements to reflector elements. A decrease in negative reactivity feedback has occurred from that shown in Table I, and is partly explained by the new reflector tubes behaving like buggy springs (Figure 19). This behavior was caused by thermal camber in the reflector ducts which, in turn, are pushing the driver ducts toward the center of the core and reducing core radius by 20 to 30 mils. The core net reactivity change is still negative for going from zero to full power because of thermal expansion effects. The buggy spring effect also pushes the reflector and blanket regions outward causing a negative reactivity.

As in Fermi, the core support pads are in compression during full power operation. If a central element should collapse in the EBR-II, the natural interference of the hexagonal array allows no significant motion of adjacent ducts toward the core center.

EBR-II emphasizes the significance of changes as small as a few mils. One mil change in core radius causes $\sim 0.72\% \frac{\Delta K}{K}$ of reactivity change. It should be noted that all of the motions shown in the graphs in this report and in Reference 12 are pictorially misleading. The net radial position change (zero to full power) for the typical EBR-II duct is roughly 4 mils, the thickness of two sheets of paper. Also, Reference 14 shows the

FUEL ROW REFLECTORS BLANKET

FIGURE 19. Motion of EBR-II Reflector Ducts

significance of a change in gap between dimples from 1 mil to 2 mils. The creep deformations at the supports lead to positive reactivity motions and this effect is discussed in parts I and J.

H. CURRENT EXAMPLE - PFR*

In PFR, the detailed core design uses vertical subassemblies with hexagonal ducts and a hexagonal array. As shown on Figure 20, control rods are evenly distributed in the array so that each control element can be fastened (at the top of the subassembly) to the six surrounding fuel ducts, creating a modular-type core of seven-element clusters.

Within a given seven-element cluster, the fuel subassemblies are preloaded against the central control rod (see Figure 21). This ensures that no gaps exist in the core region

*Information available was Limited to References 15 and 16.

FIGURE 20. *Hexagonal Array with Seven-Element Clusters*

FIGURE 21. *Preload in PFR Seven-Element Clusters*

during operation. Each cluster may be considered as a beam element with a support similar to Figure 15a. However, the thermal camber does not allow each subassembly to be at low stress during operation because the cluster acts as the stiffness-weighted average of the seven subassemblies. If each of the seven elements had the same temperature gradient, all seven elements would be at near-zero stress.

Two cases are considered. The first presupposes that the control rod is stiff in bending compared to the six fuel subassemblies. The second case involves a flexible control rod so that the fuel elements will dominate the cluster behavior. In each case, it is assumed that the control rod will have a much lower gradient than the gradient in the surrounding fuel.

Stiff Control Rod Implies:

- Very little outward motion occurs so get small negative $\frac{\delta k}{\delta P}$.
- Fuel Subassemblies become stressed during operation.
- Possible duct inelastic behavior if temperatures reach creep range.

Flexible Control Rod Implies:

- Larger outward motion occurs so obtain larger $\frac{\delta k}{\delta P}$.
- Lower stress levels exist in duct.
- Control element is now bent and subjected to high stress.

The major improvement of this system over EBR-II and Fermi is the capability to get negative reactivity feedback which is a simple linear function of coolant temperature rise across the core. In this sense the PFR resembles the FIR system described in Section III, FIR Radial Constraint Analyses.

■. STRUCTURAL DEFLECTIONS AT THE CORE SUPPORT PLANE ELEVATION

Both Fermi and EBR-II reactors utilize a system of contact pads (or dimples) for midcore support. The pads are under compression; and, in the case of Fermi, they have generally been assumed to be rigid for analytical models. Unfortunately, the pads in the EBR-II are located in the center of the flats of the hexagonal duct where local deformations can occur. In fact, the slow creep deformation in this area has been theorized as the cause of a slowly-changing, reactivity-power curve. (14) In Fermi, the contact pads are placed near the corners of the ducts and no local deformations have been reported. In EBR-II, the dimples and hexagonal ducts allow for flexibility of each subassembly at the core support elevation.

The existence of any flexibility at the core support plane also provides an additional method for positive reactivity feedback in transients. If we envision the core support as a system of fuel lumps connected by structural springs (i.e., the ducts and pads) at the core support plane, the effect of a heating transient will be as follows:

- a Heating causes increased coolant temperature and increased thermal camber of the fuel and ducts.
- a Increased thermal camber increases the compression loads on the structural springs in the system. Being like springs, each spring can only carry increased compression by having the two fuel lumps move closer to one another.
- The only way all fuel lumps can move closer to one another is by inward motion of all fuel resulting in a reactivity gain.

If the gain in temperature causes thermal expansion of the supports to occur in the same response time as thermal camber, then the expansion may offset the "compressed-spring" motion, depending upon the magnitude of the spring constant.

With pads or support rings that have slower thermal response than duct thermal camber from fuel near the core supports, it is a question of how much the fuel near the support plane will definitely move inward, causing a positive reactivity feedback. To offset the motion of fuel located near the supports, a safe system should have other fuel regions (away from the support) moving radially outward during a heating transient. Fermi designs seem to have the problem under control by using a somewhat rigid core support plane and small inflexible contact pads which can respond quickly to heating transients. EBR-II must rely mainly on the outward thermal expansion of the upper ends of the elements to offset any core support spring effects.

The FIR method uses an above-the-core support location and also allows the core to move radially outward. In addition, the contact ring above the core is extremely stiff (see Section III).

J. CREEP DEFORMATION IN THE DUCTS AND POSSIBLE CONSEQUENCES

High temperature creep may occur during power excursions in the three reactors described in previous sections. The three subject their ducts to highest stress levels during full power operation. If creep deformation occurs, some hazards will appear during subsequent reactor startups.

Note that the two cores in Figure 22 differ only in the creep deformation of core B. The tubes of core B are deformed as follows: The high bending loads compress the inner surface, extend the outside surface, leaving a tube with the central region bowed outward (when the reactor is down).

Assume a new reactor which embodies core A Configuration. Core support contact is achieved just by preloading the top of the duct, similar to Fermi methods. After operation, and with the reactor shut down (unknown to the operator), the tubes now resemble those of core B because of creep deformation in the ducts. When startup again occurs, the control rods will be

FIGURE 22. *Creep Deformation Effects in Ducts*

removed much farther because the reactor fuel is in a region of lower reactivity worth. As heating occurs during startup, the tubes of core B bow inward (see Figure 11c) producing a positive reactivity feedback until pad contact is established.

The above situation may be considered of negligible importance as long as some other structural elements are outside of the driver elements restraining the creep-deformed elements at the core support plane. The concept for the FTR has avoided the above problem by designing the core to have near-zero stresses at high temperature and by having a radially-inward preload at the core support elevation.

III. FTR RADIAL CONSTRAINT ANALYSES

REQUIREMENTS

Over the past several years, the conceptual designs for the FTR have shown quite different types of reactors. However, the designs investigated at Battelle-Northwest have generally retained the following basic features (see Figures 23, 24 and 25):

- Structural components in the core are of stainless steel.
- Fuel pins are cylinders in a hexagonal array and fission gas plenums are required.
- Fuel bundles are within 10 degrees of vertical and are placed within hexagonal ducts.
The ducts retain the sodium coolant which flows upward through the ~3-ft core.
- Duct size is 4 to 5 in. in diameter in the core region.
Since the duct must be removed from the top of the core, the larger cross-section diameters of the duct occur near the top.
- Extensions of the ducts continue on through a top cover.

FIGURE 23. FTR Split-Conical Core SK-3-14332

FIGURE 24. FTR Subassembly Duct for Split-Conical Core

SK-3-13188
(Sheet 1 of 2)
FTR-Core Driver Fuel Subassembly

- The duct complex is very tight at the elevation of the core, but the complex must also allow adequate nozzle separation at the top cover. To satisfy both requirements, the ducts gradually spread apart as they extend upward from the core.
- The fission gas plenums are placed above the core and the instrumentation packages are placed above the plenums.
- The lateral restraint of the ducts has always occurred at top cover and at one or two tubesheets roughly 4 ft below the core.
- Core is comprised of driver fuel, test facilities and control elements, each of which may be thermally dissimilar.

Within the above design constraints, the core radial support system was designed to achieve the following goals:

- A design which can easily be adapted to the various types of FIR core concepts.
- A design which can be analyzed. Motions must be repeatable and predictable.
- Movements of fuel must produce a net negative reactivity feedback effect. Small positive effects are permissible only if offset by larger negative effects of similar response time.
- Simplicity of overall reactor design and for simplicity of reactor operation.
- Optimized strain induced in core components.

B. ASSUMPTIONS FOR ANALYTICAL MODELS

The FTR core will be stable (from nuclear-structural interactions) if, for each response time, the net reactivity change is negative for a heating transient.

The proof of this postulation rests on information given in Section I-A. However, it is possible that a less restrictive assumption could also allow the design of a stable core, but

the uncertainty of stability would lead to more detailed system feedback analyses and core stability experiments. No special problems were caused by the extra conservatism.

2. *Flux and power generation curves for the FTR are as shown in Figures 8, 26, 27, 28 and 29.*

The curves show that the peak slopes appear near the outer edge of the core. The two fuel density zones (equal volumes of fuel) maintain the power output per subassembly more uniformly at the expense of having some high coolant gradients toward the edge of the core. The small changes in slope near the center of the core (Figure 8) are relatively unimportant to thermal camber reactivity change because the number of subassemblies and the radial power generation gradients (Figure 29) are small in that region of the core. The addition of poisons and moderators to the core has been unattractive from the standpoints of practical space limitations and flux penalties. (See Reference 7,8) Reflector additions of BeO have not produced smooth flux profiles near the edge of the core.

3. *Two cases of flow are studied: Uniform coolant flow, which implies the same coolant flow rate in every subassembly, and orificed flow, which assumes that the average coolant temperature gain in each subassembly is the same when going through the core.*

Both types of cores are analyzed in this section. However, the uniform flow core has been used in all FTR designs up to this time. In the uniform flow case, the power profile (Figure 8) is proportional to core axial temperature gain.

4. *Fuel material is mixed oxide with no structural rigidity.*

This allows cladding temperature to closely follow the coolant temperature. The cracked and broken oxide fuel material adds negligible flexural rigidity to the cladding.

FIGURE 26. Total Fission Rate Versus Axial Position in FTR

FIGURE 27. FTR Radial Flux Profile Along Loops

FIGURE 28. FTR Radial Flux Profile Perpendicular to Loops

FIGURE 29 Slope of Power Generation Curve (Perpendicular to Loops)

5. *Rigidity of the fuel pin bundle is negligible compared to the duct rigidity. Pins are free to expand independently in axial direction.*

This is a good approximation until friction binding or self-welding occurs between the pins.

6. *Fuel pins have negligible net reactivity feedback from motion inside the pin bundle and from pin bundle motion within the duct.*

Each pin is wire wrapped with a solid wire using a 12. in. pitch. Thus the effective length between simple supports is ~4 inches. Appendix C calculates the motion if the reactor is assumed to be at full power. The last part of the assumption does not represent the conditions at low power; however, the conditions at any high-power level should be as assumed. The fuel pin bundle has an assembly clearance with the duct diameter. When assembled some overall bundle motion can occur. However, as power rises, the duct should restrain the bundle so that no further deformation can occur. The assumed bundle shape and its reactivity effects are illustrated by Figures 5 and 6 and discussed at length in Section I-E.

7. *Duct temperatures are the same as nearby turbulent sodium inside the duct.*

The internal heating loads are disregarded and so is the low velocity sodium which leaks from the inlet plenum and circulates upward around the outside of the ducts (see Figure 23).

8. *Axial and radial temperature profiles within a duct are similar (in shape) to the curves developed by M. T. Jakub and A. Padilla (Figures 1 and 30).*

Temperature patterns in the coolant are discussed in Appendix D. Note that no large gaps are allowed near the edge of the duct in the analysis of the Appendix D. This should be true for wire-wrapped fuel.

FIGURE 30. Temperature Difference Across Subassembly Flats

9. *No buckling phenomena are expected in the pins or ducts.*

This seems reasonable since current FTR designs use hydraulic balancing to eliminate duct axial loading. The pins are supported at the bottom of the core to ensure that they are in tension. However, binding or self-welding of pins could change the pin problem.

10. *Material used in pins and duct has constant α , E values in the temperature range of interest (500 °F to 1200 °F).*

For stainless steel, α increases and E decreases with increasing temperature. In stress calculations, the product, (αE) , is the important quantity; and it remains more constant. For example, 500 to 900 °F changes the product by only a few percent. With deflections, the α values are the only factor, and, an average for the temperature variation at a given point should be selected. Note that the more general case of variable α is not too difficult to handle using the methods of Appendices A and B.

11. *The temperature profile across a duct will be assumed linear with radial position in the core.*

This allows the great simplification of thermal camber equations noted in Appendices A and B. In fast reactors, the flux profile does not have strong localized perturbations (as in thermal reactors) and is relatively smooth for the 4- to 5-in. duct diameter distance. Equation (12) can now be applied to the FTR system.

12. *Elastic analysis of thermal camber is valid.*

Stainless steel is elastic material for the temperature range, especially when the stresses remain low at higher temperatures.

13. *Duct bending can be analyzed by simple beam theory and by superposition of loads.*

With an elastic system and no axial loads to initiate duct buckling, the linearly elastic system model is appropriate to within a small percent of error. This error would be the result of the difference in shear stress distributions in the tube versus the simple beam.

14. *Rigid supports exist at all support points, including the core support planes.*

The rigidity of the tubesheets to in-plane deformations is certainly true to within 1 mil. However, it is also necessary to examine the duct at those points. Figure 31 illustrates how the transition is reinforced at the core support plane. The wall thickness is more than doubled for the hex contact ring. Using the circular ring loaded by two opposed forces as an example, doubling the thickness will reduce the deflection by a factor of 8.

At the upper tubesheet, local reinforcement of the contact wall is allowable. At the lower tubesheet, the reduced duct diameter gives extra stiffness so that the duct wall can probably remain unreinforced. Actually, any outward structural deformation of the duct at the upper and lower tubesheets will provide additional negative reactivity feedback. Thus the "rigid support" assumption is conservative for these two supports.

15. *Pinned (or simple) supports exist at the upper tubesheet and the (uppermost) lower tubesheet.*

Early in the FIR radial restraint work, it was decided that guaranteeing a simple support at the top and bottom of the duct was easier than guaranteeing a perfectly built-in end condition." The present FIR concept does not have a completely

* *Tolerances become too tight and gaps usually exist for built-in ends.*

FIGURE 31. FTR Subassembly Duct - Thick Hexagonal Bearing Rings

moment-free support (see Figure 32). However, the test program should show that the moments are negligible. Obviously, as the end supports become more like built-in supports, the stresses become higher and the thermal camber deflections will decrease.

Of the several pinned supports, the top cover and the lower tubesheet supports are not expected to play a significant role in core stresses and deflections. If the tubesheet thickness is 4 in., then, assuming a preload of 0.4 in. at the core support plane, the slope at the end will require a total diametral gap of 0.040 in.

At the upper end of the duct in the top cover region, the expected rotations would require ~ 1 mil change in the deflection of a gasket on a flanged joint. This is acceptable for simple support assumptions. If a guide tube is used, the guide tube can possibly interfere with the rotation of the duct.* This influence was disregarded in the analysis -- and the actual tube should be able to eliminate any influence by either allowing 0.200-in. diametral clearance at the lower extremity, or by making the duct region above the outlet have more flexibility.

16. The response times of interest are shorter than any thermal expansion effects at supports. Thus all thermal expansion effects (except thermal camber) may be disregarded.

By referencing the subassembly at the inlet plenum and the fuel at the bottom of the core (Figure 21) the only thermal expansion in the axial direction for a heating transient will cause a growth of the core volume and a decrease in reactivity. This design is currently considered the best choice for the FTR.

* Guide tube is a tube attached to the top cover and enclosing the subassembly duct. This tube provides a guide for inserting a subassembly into the reactor (see Figure 23 and 33).

SK-3-14330

FIGURE 32. Lower Plenum and Lower Tubesheets

From
SK-3-14331

FIGURE 33. Contact Points on Duct

If the subassembly support is at the top cover of the FTR, then a small positive reactivity effect occurs, from a heating transient (see Appendix E). This is a small effect and is easily overshadowed by the thermal expansion growth in the radial direction which is a large negative reactivity feedback. This follows from an expansion behavior in the radial direction much like the one in the axial direction.

The size and location of radial supports for the ducts will determine the response time for the FTR fuel radial motion. Size influences thermal inertia, and location determines the coolant transport lag time from the core. With a mutual contact plane for all ducts at the core elevation (such as in Fermi) and with small thin buttons for the actual contact, the radial response time would be close to the response time of the ducts. However, the FTR duct designs have utilized only contact planes with much thicker members than the duct wall (see Figure 31).^{*} The response times of interest to feedback analysis of the FTR can be broken down into the following categories:

- a. <0.1 msec - The heating has time to affect only the fuel. There are no cladding or other structures involved. The Doppler coefficient is important as well as neutron leakage changes.
- b. 0.010 to 10 sec - The cladding and duct (as well as coolant) have time to receive substantial heating from the transient. Thermal camber and pin effects occur here.
- c. 1 sec to ~1 min - Thermal expansion effects dominate this time period as the coolant has time to heat large structures exposed to outlet coolant.

^{*} *The only exception is where the core contact plane was placed at the bottom of the core; where the unchanging inlet conditions ensured that no structural support expansion occurred during transients.*

Each of the above categories can be analyzed independently if our goal is to show that each time regime has no net positive reactivity feedback phenomena occurring in a heating transient. The first category does not involve structural components in oxide-fueled reactors. The last category has only negative feedback since any fuel expansion in the radial direction

- Increases core volume, thus decreasing fuel density, which is a negative feedback.
- Moves fuel out into the reflector region which is a lower worth region for FIR flux profiles (Figure 25).

The analytical model will conservatively disregard all thermal expansion effects at the supports.

17. The reactivity worth (per unit plutonium fuel) is proportional to the square of the local flux.

The "worth" curve is plotted along with appropriate flux curves in Figures 9 and 10. All of these curves were calculated by L. Maas for a two-zone FIR model. The error of assumption 17 is very small.

Figure 34 displays the FIR final model in a comparison to EBR-11 and Fermi models. The one difference seems to be the radial temperature gradients. The FIR disregards any change in radial gradients of the coolant until a flow mixer is reached above the gas plenum. This has been verified by A. Padilla's analysis (Appendix D and Figure 30). Several different temperature profiles are analyzed in later parts of this section.

The core radial support plane was finally located at the transition zone above the core. The following parts of this section review the calculations that led to this decision.

18. Each driver duct deflects independently of the deflections of other ducts.

FIGURE 34. Comparison of Three Duct Models

If the support points for a given duct can be assumed rigid and without any radial gap, deflections of the duct will be independent of all other duct deflections. In the FTR the rigid supports will exist if: There are no radial deformations of the upper and lower tubesheets, and if none of the subassembly core support rings are deflected. Radial gaps will be eliminated by the preloading of the FTR core. Finally, no new contact points may be established between ducts in the course of reactor operations. At the bottom of the core the gap between adjacent ducts is ~30 mils, but thermal camber will not close this gap because duct lateral motion is increasingly outward for ducts toward the outside of the core. In Table I of Appendix F under the ΔR column, the relative thermal camber deflections are shown for increasing radial position in the core.

C. FTR-DUCT ANALYTICAL MODEL

A basic requirement (part A) was that heating transients must lead to negative reactivity feedback deformations, i.e., radially outward from the core center. This sign convention will be used in all subsequent figures and discussions. If positive deflections do occur in portions of the active core zone, they must be offset by larger negative deflections in other portions of the core at the same instant in time.

The Structural Analysis and Matrix Interpretive System (SAMIS) was used to model the flow ducts analytically. Figure 35 shows the pertinent geometrical properties and radial temperature profiles. The assumed radial temperature profiles B and C represent two different locations of the flow mixer. Profile A represents a minimum thermal camber profile which could not occur in the present design because of poor coolant mixing in the fission gas plenum.

The numbers 1 through 30 in Figure 35 denote the finite element breakdown used in modeling the flow ducts. In regions

FIGURE 35. Basic Geometry of Flow Duct and Temperature Profiles

where geometrical properties are changing and the radial temperature gradient exists, a refined nodal breakdown is used. Each span between node numbers is assumed to be a beam with constant cross-sectional properties and a radial temperature gradient representing the average of the gradients at the nodal end points. The fuel subassembly is assumed to move with the flow duct and to not contribute significant structural stiffness. For end supports, the flow duct was assumed to be simply-supported at the top of the reactor cover and at the lower tube-sheet. Subsequent figures denote any geometrical changes in the flow ducts from the geometry indicated in Figure 35.

D. UNRESTRAINED THERMAL BOWING BEHAVIOR

To assess the severity of the thermal bowing problem, the flow duct was analyzed without any intermediate constraints. Figure 36 shows the positive radial deflection inward toward the center of core induced by temperature profiles A and B. Temperature profile A with a maximum ΔT of 238 °F resulted in a maximum radial deflection of 1.156 inches. Temperature profile B with a maximum ΔT of 100 °F resulted in a maximum radial deflection of 0.655 in. These unrestrained deformations would cause an excessive positive reactivity effect and must be alleviated by the proper placing of intermediate radial constraints.

INTERMEDIATE CORE SUPPORTS AND ASSOCIATED GEOMETRICAL MODIFICATIONS

To establish the proper location of supports and the dimensional proportions of the flow ducts to produce a net negative reactivity effect, a parameter study was conducted. Two major modifications in the geometrical design were associated with outlet zone concepts. The Type I outlet zone consisted of a tubular section with reduced diameter and a wall thickness ($t = 0.15$) (see Figure 37). In contrast, the Type II outlet zone consisted of a perforated tube which was assumed to have a wall thickness double that of the rest of the duct. For the

FIGURE 36. Thermal Bowing of Flow Ducts Without Intermediate Constraints

FIGURE 37. Type I Geometry of Flow Duct

Type I outlet, the weakening of the flow duct in this region required the placing of an additional support (tubesheet) at node 27. The Type I design was conceived as having the following advantages:

Resistance to coolant crossflow at the outlets of the subassemblies would be reduced, thus central hotter tubes would not need extra pressure drop just to move the coolant to the outside of the duct complex.

- The reduced coolant crossflow velocities and the extra support point would minimize the probability of fluid-induced duct vibrations.
- The flexibility of the small diameter action would allow the support at node 27 to remain fixed without causing major increases in duct stress level.
- The shorter total length of the duct would mean less deflection of the duct at the core support plane elevation.

Three intermediate core support locations were attempted for both of the above outlet designs. Geometry changes associated with each of the support locations are indicated on the specific figures. If sections of the flow duct are not designated as being changed, then they remain the same as indicated on Figure 35.

1. Top of Pin Bundle [Node 25]; Bottom Flexible, Top Stiff

Figures 38 and 39 represent Type I and II outlet zones, respectively, with an intermediate core support at the top of the pin bundle (Node 25). The bottom portion of the flow duct is relatively flexible compared with the top portion. With this type of flow duct constraint, a net negative reactivity effect could not be obtained for any of the geometrical configurations indicated in Figures 38 and 39.

FIGURE 38. Intermediate Support (Node 25) at Top of Pin Bundle, Type I Outlet Zone

FIGURE 39. Intermediate Support (Node 25) at Top of Pin Bundle, Type II Outlet Zone

2. Bottom of Active Core (Node 10); Bottom Stiff, Top Flexible

Figure 40 represents a case for which the bottom portion of the flow duct has been stiffened and the top made flexible with the intermediate support just below the active core at Node 10. This type of support and dimensional proportioning again was not successful in obtaining a net negative reactivity effect.

3. Transition from Hexagonal to Round Tube (Node 20); Bottom Flexible, Top Stiff

Figures 41 and 42 represent Type I and II outlet zones, respectively, with an intermediate core support at the transition zone from hexagonal to round tube (Node 20). Temperature profile (B) was used for the results presented in Figures 41 and 42. For the Type I outlet zone (Figure 41) either of the dimensional configurations considered gave a negative reactivity effect. However, for the Type II outlet zone (Figure 42) the upper portion of the flow duct had to be stiffened ($t = 0.30$) as indicated to obtain a total negative reactivity effect. With the upper wall thickness of 0.15 in., there was essentially a balance between the positive and negative reactivity effects.

Since there exists a possibility that coolant mixing may not occur before reaching the instrument package, the effect of temperature profile (C) upon this particular intermediate support system and dimensional proportions was considered. Figures 43 and 44 record these results for Type I and II outlet zones, respectively. In this case for the Type II outlet zone, even the upper portion wall thickness of $t = 0.15$ was sufficient to give a total negative reactivity effect. Thus, this intermediate support system at node 20 represents a conservative design since the extension of the radial temperature gradient up to the instrument package introduces only an additional negative reactivity effect.

FIGURE 40. Intermediate Support (Node 10) Just Below Active Core with Bottom Stiff and Top Flexible

FIGURE 41. Intermediate Support (Node 20) at Transition from Hex to Round Tube, Type I Outlet Zone

FIGURE 42. Intermediate Support (Node 20) at Transition from Hex to Round Tube Type 11 Outlet Zone

FIGURE 43. Intermediate Support (Node 20) at Transition from Hex to Round Tube, Type I Outlet Zone

FIGURE 44. Intermediate Support (Node 20) at Transition from Hex to Round Tube, Type II Outlet Zone

- A tapered hexagonal tube whose wall thickness decreases as the tube is transversed from the small diameter to the large diameter end is easier to fabricate. This wall thickness variation is opposite to that indicated for the tapered hexagonal tube in Figure 31 and used in all previous analyses. Thus, Figure 45 represents a case for which the tapered hexagonal tube wall thickness is reversed to see if it has an adverse effect on the net reactivity effect. The geometrical modifications are designated on Figure 45. Comparison with Figure 37 shows that this reversal does not have an adverse effect on reactivity.
- As the lower tubesheet support model changes from "pinned" toward a "built-in" type, the effect on duct deflections will be the same as the above case with increased stiffness in the bottom section. As gaps decrease below 40 mils at the lower tubesheet, the change in duct deflections will also be small as suggested by the above results.

F. PRELOADING TO REDUCE STRESSES

The results presented in Figures 41, 42, 43, and 44 indicate that an intermediate support placed at the transition from the hexagonal to round tube (Node 20) produces a negative displacement leading to a net negative reactivity effect. This constraining of the flow ducts to deform in a prescribed fashion leads to bending stresses which could adversely affect the creep behavior if allowed to persist. The bending stress levels at full power were reduced by preloading. Preloading cases were considered for both Types I and II outlet zones.

Figures 46 and 47 show the effects of preloading for Type II outlet zone. If the flow duct is preloaded with a load of 207 lb, it will assume an initially bowed shape as shown in Figure 46. With temperature profile B imposed, the bowed configuration at full power will be as shown. This configuration

FIGURE 45. *Reversal of Tapered Hexagonal Wall Tube Thickness, Type II Outlet Zone*

FIGURE 46. Preload Support Point (Node 20), Type 11 Outlet Zone

FIGURE 47. Bending Moments Associated with Support at Node 20, Type II Outlet Zon.

is essentially stress free as indicated by the full power moment distribution in Figure 47. Without preloading, the full power moment distribution would have been as indicated in Figure 47. Thus, preloading has resulted in the full power maximum bending stress being reduced from 5150 psi to 32 psi in the active core zone.

Figures 48 and 49 show the effects of preloading for a Type I outlet zone. For this particular study the outlet zone was extended an additional two feet over previous Type I outlet zones. Preloading the flow duct with a load of 425 lb resulted in an initially bowed configuration as shown in Figure 48. The bowed configuration at full power with temperature profile B imposed is also shown in Figure 48. In this case there is a total negative reactivity effect. The full power moment distributions with and without preloading are indicated in Figure 49. Thus, for a Type I outlet zone, preloading resulted in a reduction of the full power maximum bending stresses from 7000 psi to 410 psi in the active core. A significant advantage of the Type I outlet zone is that the maximum preload deformation is only 0.378 in., whereas, for the Type II outlet the maximum was 0.715 in.

G. CONCLUSIONS FROM MODELS

Initial results indicated that it was absolutely necessary to have some type of intermediate support system to relieve the thermal bowing of the flow ducts induced by the existing radial temperature gradients. Subsequent analysis indicated that the specific location of the intermediate supports was critical in obtaining the desired negative reactivity effect as the reactor was brought up to full power. Final results indicated that an intermediate core support at the transition zone from hexagonal to round tube (Node 20) one foot above the top of the active core zone would provide the desired net negative reactivity effect. This intermediate support at Node 20 was

FIGURE 48 Preload Support Point (Node 20) Type I Outlet Zone

FIGURE 49. Bending Moments Associated with Support at Node 20, Type I Outlet Zone

effective in producing the desired reactivity effect for either Type I or II outlet zones. However, the Type I outlet zone is preferable because of the lessening of the cross flow vibrations problem. Furthermore, the Type I outlet requires less maximum preload deformation (0.378 versus 0.715 in.) to relieve the bending stresses at full power.

Type I is the current FTR choice for the split conical design. If a skewed core design* is ever used, the changes in the radial core constraint system will be minor. Specifically, the core support plane would be lowered to the top of the core and the contact pads would no longer be true hexes because of the skew angle. With a vertical core FTR design, the duct would end at the outlet region, and the upper tubesheet support would have to depend upon a tube-to-tube contact plane (similar to EBR-II). Thus the Type I core radial support system is easily adaptable to various types of FTR cores and the temperature and deflection profiles will be similar to the above results; only the magnitudes will change.

H. REACTIVITY EFFECTS

From Figure 41, the deflection of the fuel is very close to linear over the 3-ft core length, thus the radial deflection of the fuel is directly proportional to the magnitude of the thermal camber profile. Figure 6 shows that all of the necessary conditions exist for analyzing the reactivity changes by the simple equations of subsection I-J.

From Appendix F, we see that the net reactivity change when going from zero to full power is

0.113% 6K if uniform flow core
0.123% 6K if orificed flow core

**Pinch plane location is at mid-core elevation to maximize fuel density.*

From these numbers, which correspond to a temperature rise of 350 °F, we can obtain temperature coefficients for reactivity which would apply to any transient which starts from a high-power, steady-state condition

$$\% \delta K \text{ per } ^\circ\text{F} = \begin{cases} -0.323 \cdot 10^{-3} & \text{uniform flow} \\ -0.351 \cdot 10^{-3} & \text{orificed flow} \end{cases}$$

(coolant rise across core)

As a rough comparison to other reactivity effects, it is assumed that the thermal expansion of the core from zero to full power is approximately -1% δK . Thus, the thermal camber may act faster, but it is an order of magnitude less than expansion feedback phenomena as shown on Figure 50. This result shows that the thermal camber deflections are localized in the outside rows of tubes (see Appendix F) and the maximum deflection is ~32 mils. The uniform thermal expansion has a maximum of

$$\Delta r = \alpha T r_{\max} = 10^{-2} (350)(26) = 91 \text{ mils}$$

Thus thermal expansion feedback should exceed thermal camber feedback.

■. SUBTLE AND NOT-SO-SUBTLE PROBLEMS

The amount of preload deflection is selected on the principle of achieving the lowest stress at full power operation. The preload forces per tube have currently been calculated to be in the 300 lb range. Since this is a significant load, the influence of deflections, transmission of force to inner tubes, duct radial temperature profiles, and sticking can lead to loads much higher than 300 lb.

If radial temperature gradients are not terminated immediately above the pin bundle (usually by a flow mixer), then the wall gradients would be extended for up to five additional feet. This will lead to increased thermal camber, which should be offset by additional preload forces and deflections. Note, though,

FIGURE 50. Thermal Expansion and Thermal Camber in the FTR

that the fuel motion also increases its negative feedback as the radial temperature profile elongates (profile C versus profile B, Figures 31 and 36).

If preloading is performed at the edge of the core, then the reflector tubes must be deformed and must transmit forces to deflect all inner tubes. The reflector tubes should be much more flexible than the driver ducts. Yet these tubes should have a rigid ring section to transmit the loads to the driver ducts. The two conflicting requirements, strength with flexibility, can be met by using thin-walled tubes, with a thick ring section at the core support section. All of the reflector material inside the tubes (e.g., nickel) must be flexible and this implies using short rod bundle lengths.

As discovered in EBR-II, guaranteeing the radial temperature profiles in the reflector is often difficult. Even with mixing devices, reflector tubes will be deflected by thermal camber loading. These deflections will be troublesome only if they are inward at the core level and are large enough to start forcing the driver assemblies inward. In particular, the closest point could easily be the bottom of the core where outward bowing of the driver ducts is maximum. If determining the magnitude of reflector tube thermal camber is difficult, the direction must be controlled and outward at core level.

The outer rows of driver ducts must transmit preload forces to all inner tubes. Fortunately, the preload deflections rapidly fall to zero as core radius decreases. For example, the preload deflection on an orificed core would be proportional to the amplitude of the curve in Figure 29. The outer two subassembly rows (i.e., the higher fuel density elements) will have the important deflections.

The preload forces can be reduced by reducing the moment of inertia, I , where

$$I = \text{constant} \cdot tD^3$$

Obviously, reducing the tube diameter is the quickest solution; reducing the wall thickness is a second alternative. Reducing the diameter of the flexible section above the outlet gives an immediate reduction in preload forces. The tube diameter above the outlet has not been reduced to the lowest possible value (i.e., where axial loads give barely acceptable stress levels). The reason is that the duct (~600 lb) becomes rather awkward to handle.

Once the tubes are all in core, the tubes must be preloaded radially inward by a preload device located outside the core. To ensure contact between all subassemblies when the radial preload device acts, it is not enough to make all subassemblies the same size. Individual tolerances in a cluster of ducts will allow six ducts to surround a slightly smaller duct leaving this middle duct free to move laterally for a short distance. As many as 18 tubes in a 127-tube core may be in this slightly free condition.

Two methods ensure tightness and eliminate bridging. Flexible gap-filling devices may be located on every seventh tube, or every seventh tube may be smaller and nonfueled. Actually, both methods seem to require that every seventh tube be nonfueled; Figure 51 shows that the current FIR design indeed does this. If it seems impossible to put a flexible finger device in driver fuel tubes, then making a practical similar device in control rods is almost equally difficult.

A second alternative makes use of smaller diameter control rods (safety rods, shim rods, etc.). Since these elements have little or no thermal camber deflections, they would be unnecessary burdens on the radial constraint system. Also, it is not

TENTATIVE INITIAL TESTING FACILITIES

⊗ REFLECTOR SHIM ROD (8)	○ REFLECTOR OPEN TEST POSITIONS (39)
⊗ SHIM SAFETY ROD (6)	⬢ DRIVER FUEL (77)
○ SAFETY ROD (4)	⬡ INCORE OPEN TEST POSITIONS (11)
⬢ CLOSED LOOP (5)	⬢ SHORT-TERM TEST (1)
● NEUTRON SOURCE (1)	
~ OSCILLATOR (1)	

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FIGURE 51. *Tentative Core for Split Conical Design*

desirable to constrain and bend control rods. If they are free, their radial motion will not involve significant reactivity feedback effects.

The second unusual problem involves fast transient thermal stresses. In some coolant temperature transients such as a reactor scram, there is a short distance above the core in which thermal shock cannot be reduced significantly even with automatic shutdown or primary pumps. In this region, large structural members must be less than ~ 0.35 -in. thick and for small pieces not more than 1 in.^2 of surface area, the thickness should be less than ~ 0.5 -in. thick if the part is to endure repeated transients successfully. The area at the core support plane approaches the critical thicknesses.

J. PRELOAD DEVICES

Preloading of all ducts at the core support elevation requires a special device. The requirements include:

- Inward motion must be slow and controlled since it is a positive reactivity addition to the core. Under no circumstances may it be preloaded rapidly.
- The devices must be "fail-safe"; that is, failure will release the preload forces.
- The device must be remotely controlled since it has major parts around the core.
- a Overriding considerations in selecting the final design must be simplicity in design, safety and reliable operation.
- High neutron flux precludes using electronics and thick structural components near the core.
- Any part exposed to the coolant must be of stainless steel or stainless steel plated.
- The core may expand radially from thermal expansion, but the device must retain enough flexibility to allow this expansion without undue increases in preload forces.

- The actuating method for preloading must not depend on temperature since a heating transient could cause positive reactivity feedbacks.
- The device must not overload (i.e., collapse) a given edge tube because of tolerances or other causes of uneven loading.

Figure 52 represents one example of a simple preload device. Drive motors located above the top cover simply raise and lower a shaft with a wedge end. The actual preload forces are applied by a series of beam-like members which rotate about a pivot point.

Another alternative for driving the preload device is to use the inlet coolant pressure. This system has not reached a completed design at this time. This system and, hopefully, several others will also be tried before the optimal method is selected for the FTR.

K. PROPOSED TESTS

1. It seems reasonable to expect great difficulty in checking the radial temperature profile (Figure 1); however, the action of the pin bundle on the duct must be tested. Can the pin bundle become self-welded or develop frictional forces that will influence the duct-bundle rigidity? Assuming that the radial profiles are as shown, the rigidity effects would have a stress-free profile similar to that of the duct. The problem, in this case, is the additional stresses in the duct.

2. The optimal core radial preload device (Part J) will be determined only after some development tests. A sealed or full-sized mockup of a section of the core would provide a suitable experimental model for the current preload device. Hopefully, all other radial constraint systems can be tested on the same mockup after slight modification.

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FIGURE 52. Lateral Restraint Mechanism

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3. The same test mockup should also enable a complete evaluation of the interactions at the core support plane. Will reducing the diameter of every seventh tube really work? Will the flexible finger devices have any success? Such questions must be answered unless reactor operation is dependent on unproven systems.

4. The wear characteristics of the duct need to be evaluated. Such places as the hard-faced regions must be checked for galling resistance and self-welding in the hot sodium coolant.

5. Finally, all changes in the duct design must be evaluated in light of the assumptions and results of this report. Any uncertainties or phenomena changes will need proof testing.

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APPENDIX A
THERMAL CAMBER EQUATIONS

APPENDIX A

THERMAL CAMBER EQUATIONS

This analysis will be confined to a long, perfectly elastic beam of a cross section having symmetry about the axis of temperature variation. This category includes cylindrical tubes, hex tubes and square tubes -- the figure illustrates the rectangular beam.

Using a standard method," temperature is expressed as the sum of a constant, a linear variation, and an even and odd functions of y

$$T(y) \equiv a + by + f_{\text{EVEN}}(y) + f_{\text{ODD}}(y) \quad (1)$$

where y has its origin at the centroid of the member

The lateral displacement y, in the y direction is then described by the equation

$$\frac{d^2 v}{dx^2} = \frac{M_T(y)}{E I_z} \quad (2)$$

where plane sections are assumed to remain plane, and

$$\text{where } M_T(y) \equiv \iint_{\text{Area}} \alpha E y T(y) d \equiv dy \quad (3)$$

α = coefficient of linear thermal expansion

E = Young's Modulus of elasticity

I_z = Moment of inertia about z axis

*See Boley Weiner, Theory of Thermal Stresses, Wiley (1960) for example)

Substituting for $T(y)$ in (3) gives non-zero results only for odd functions if it is assumed that a , E are constants across the given cross section.

$$M_T(y) = \alpha E \int \int (by + f_{\text{ODD}}(y)) y dz dy = b\alpha E I_z + \alpha E \int \int_{\text{area}} f_{\text{ODD}}(y) y dz dy \quad (4)$$

Note that $M_T(y)$ has not been restricted from having an x variation. Boley and Weiner (see foregoing footnote) show that elementary beam theory gives the exact answer if the axial variation is either constant or linear. Only small errors are introduced by having axial profiles that are very smooth, and this is generally true of reactors with high coolant velocities.

For the axial component of stress in the unrestrained beam,

$$\sigma_{xx} = -\alpha E T(y) + \frac{\alpha E}{A} \int \int_{\text{Area}} T(y) dz dy + \frac{M_T y}{I_z} \quad (5)$$

Using equation (1), the above equation reduces to

$$\sigma_{xx} = -\alpha E \left\{ f_{\text{EVEN}} - \frac{1}{A} \int \int_{\text{Area}} f_{\text{EVEN}} dz dy + f_{\text{ODD}} - \frac{y}{I_z} \int \int_{\text{Area}} f_{\text{ODD}} y dz dy \right\} \quad (6)$$

For linear temperature variation, the bracket term becomes zero; and the unrestrained, but deflected, beam has no stress. Note also that the arbitrary constant temperature, " a " in (1) has no effect on stress or deflection -- which is to be expected.

APPENDIX B

THERMAL CAMBER CONCEPTS FROM A STRENGTH OF MATERIALS APPROACH

APPENDIX B

THERMAL CAMBER CONCEPTS FROM A STRENGTH OF MATERIALS APPROACH

Take an element of the beam which has a linear temperature gradient across the depth of the beam. Assume $\alpha = \text{constant}$

But $\theta = \frac{\Delta x}{R}$, thus the radius of curvature, R , can be defined in terms of the temperature difference $\Delta T = T_2 - T_1$.

$$\alpha \Delta T \Delta x = \theta \left(R + \frac{h}{2} - \left(R - \frac{h}{2} \right) \right) = \theta h = \frac{h}{R} \Delta x$$

$$\frac{1}{R} = \frac{\alpha \Delta T}{\Delta x}$$

Note that all the terms on the right-hand side of the equation may vary as a function of the beam position variable, x . It is assumed that simple beam bending equations apply to the beam -- thus restricting the x variation to constant or linear. From the usual strength of materials formulation, we obtain

$$\frac{M}{EI} = \frac{1}{R} = y''(x)$$

Thus, the equivalent position of the varying temperature beam is the deflection from a moment distribution:

$$M(x) = \frac{E(x) I(x) \alpha(x) \Delta T(x)}{h(x)}$$

For a reactor tube under the linear radial temperature gradient caused by the radial fall-off of power generation, the E, I , and h are often constants for a fixed geometry beam having small changes in values for the temperatures in the beam. Thus,

$$M(x) = \frac{EI\alpha}{h} \Delta T(x)$$

If the slope of the temperature curve is approximately a constant with increasing radius for the distances of the tube, then the slope, K , is just $\frac{\Delta T}{h}$, and

$$y'' = \alpha K(x)$$

which agrees with equation (6) of the text. The above equation would hold in any reactor situation where the power generation curve and the temperature are linear with the radius.

When the appropriate moment equation for thermal camber has been determined, simple beam theory can be used with the appropriate bending moments adding together. Note that the thermal camber moment determines the stress-free profile of the duct; stress is then determined by moments from the external forces in a redundantly supported beam. As usual, the stress at a given position is just the moment at the cross section times the distance from the neutral axis divided by the moment of inertia, $[M_y/I]$.

APPENDIX C

THERMAL CAMBER OF FUEL PINS WITHIN FUEL PIN BUNDLES

APPENDIX C

THERMAL CAMBER OF FUEL PINS WITHIN FUEL PIN BUNDLES

A static, thermal analysis on the conceptual FTR driver fuel pin was performed assuming simple supports every 6 in. The fuel pin is wire wrapped in such a manner that opposite sides of the pin are supported every 6 in. in the same plane. Opposing supports from the wire wrap occur every 2 in., but not in the same plane. Thus, the fuel pin is actually constrained more than the analysis for the 6 in. support spacing assumes. As a result the analysis is considered to be conservative.

The geometrical details of the fuel pin were taken from Drawing SK-3-13187 and Figure 29. Figure 8 shows the simple support locations and the associated radial temperature change AT across the pin.*

Figure 9 shows the radial displacement and bending moment associated with the temperature distribution indicated in Figure 8. The maximum AT was 37 °F, resulting in a maximum radial displacement of 0.156 mils. The maximum bending stress associated with the moment distribution shown in Figure 9 was 4200 psi. Thus, it appears that the wire wraps do an excellent job constraining the fuel pin against thermal bowing without introducing excessively high thermal stresses.

Simpler beam models can also be used to obtain approximations to deflections. The previous problem is approached by noncomputer solutions in the remainder of this appendix.

"Temperature data was taken from a memorandum by R. J. Jackson, "Stress Analysis of Fuel Pin PNL VIII", August 25, 1967.

Assume that the fuel pin bundle is spaced by wire wrapping of one-third of the pins. An analytical model for bowing can be the following:

Using the symmetry about point A, the maximum deflection will be the end deflection of the following simple beam model.

It should be noted that the choice of a fixed end does not greatly influence the deflection at point A. The force P now represents the nearest wire wrap support (i.e., zero deflection) and M is the symmetry condition at the end (i.e., zero slope).

Total displacement toward the center of the channel is $\delta/\sqrt{3}$.

The temperature profile will be considered as linear between the outside and the inside coolant channels. The worst bending will occur if the radial temperature difference occurs in a region between two support wire wraps. Assuming a constant radial temperature difference between two wire contacts, the net effect of the temperature difference will be a moment as shown in the figure below:

This profile is more applicable to local flow blockage accidents.

The magnitude of the thermal camber moment (see Appendix A, for derivation) is:

$$M_T = \frac{EI\alpha_T\Delta T_r}{OD}$$

The end deflection is again $\delta/\sqrt{3}$, thus,

$$\Delta T_r = \frac{(\delta/\sqrt{3})(OD)}{\alpha_T L^2 \alpha \beta} \left[\frac{1/4 + (1/3) a/b}{1/9 + (1/8) a/b} \right]$$

If $\alpha_T = 10^{-5} \text{ in./in. } ^\circ\text{F}$, $OD = 0.25 \text{ in.}$, $a = 6 \text{ in.}$, $b = 12 \text{ in.}$,
 $6 = 28 \text{ mils}$

$\Delta T_r = 13.5 ^\circ\text{F}$ ($7.5 ^\circ\text{C}$), Deflection rate = $1.2 \text{ mils}/^\circ\text{F}$

If the unsupported length is reduced from 12-inch to 4-inch by wrapping every pin on a 12-inch pitch, then,

$\Delta T_r = 122 ^\circ\text{F}$. Deflection Rate = $1.2 \text{ mils}/^\circ\text{F}$

For the 6-inch spacing (similar to detailed analysis)

Deflection Rate = $0.3 \text{ mils}/^\circ\text{F}$.

If triangular moment load (Figure 9) is replaced by constant load in the above model, then $1\ 1/2\ ^\circ\text{F}$ is the corresponding constant ΔT_r , and max. deflection = 0.45 mils

Using even more elementary models, the deflection curve of Figure 9 should lie between the following two cases:

$\equiv$		$W = \frac{4EI\alpha\Delta T}{\ell(OD)}$
$\equiv$		$W = \frac{4EI\alpha\Delta T}{\ell \cdot OD}$
			$\delta = \frac{\Delta T_{\max} \ell^2 \alpha}{48 OD} = \frac{(3)(6)^2 \times (10^{-5})}{48 (1/4)} = \underline{\underline{0.09\ \text{mils}}}$
			$\delta = \frac{\Delta T_{\max} \ell^2 \alpha}{12 OD} = \underline{\underline{0.36\ \text{mils}}}$

These models successfully bracketed the detailed analysis answer of maximum deflection = 0.16 mils. However, the lesson of the previous model should not be overlooked -- local flow blockages can produce deflections much larger than ordinary (or normal) temperature profiles.

APPENDIX D
TEMPERATURE PROFILES IN AN FTR DUCT

APPENDIX D
TEMPERATURE PROFILES IN AN FTR DUCT

Let $q(x) \equiv$ Heat transfer rate
to coolant at posi-
tion x per unit
axial length.

For no mixing, each fluid ele-
ments (1,2,3) have temperature,

$$T_i(x) = T_{\text{inlet}} \Big|_i + \int_0^x q_i(x) dx,$$

Assume

$$\begin{aligned} \frac{q_{\text{max}}(x)}{q_{\text{avg}}(x)} &= \frac{\text{power flux} \Big|_{\text{max}}(x)}{\text{power flux} \Big|_{\text{avg}}(x)} \\ &= \frac{P}{A}(x) \end{aligned}$$

Then if all fluid has the same
inlet temperature,

$$T_{\text{avg}}(x) = T_{\text{inlet}} + \int_0^x q_{\text{avg}}(x) dx$$

$$\begin{aligned} T_{\text{max}}(x) &= T_{\text{inlet}} + \int_0^x q_{\text{max}} dx \\ &= T_{\text{inlet}} + \int_0^x \frac{P}{A}(x) q_{\text{avg}} dx \end{aligned}$$

Assuming equal deviations of temperatures from the average
coolant temperature,

$$T_{\text{min}}(x) = T_{\text{inlet}} + \int_0^x \left(1 - \left[\frac{P}{A} - 1\right]\right) q_{\text{avg}} dx$$

Radial temperature drop is $\Delta T_r = T_{\max} - T_{\min}$

$$\Delta T_r(x) = \int_0^x \left[\frac{P}{A} - \left(2 - \frac{P}{A} \right) \right] q_{\text{avg}} dx = 2 \int_0^x \left[\frac{P}{A} - 1 \right] q_{\text{avg}} dx$$

If L is the length of the core and ΔT_{axial} is the average ΔT ,

$$\Delta T_{\text{axial}} = T_{\text{avg}}(L) - T_{\text{inlet}} = \int_0^L q_{\text{avg}}(x) dx$$

Thus, the maximum radial temperature difference is determined if the peak-to-average flux is a constant over the entire length of the core,

$$\Delta T_r \Big|_{\max} = 2 \int_0^L \left[\frac{P}{A} - 1 \right] q_{\text{avg}} dx = 2 \left[\frac{P}{A} - 1 \right] \int_0^L q_{\text{avg}}(x) dx$$

or

$$\Delta T_r \Big|_{\max} = 2 \left[\frac{P}{A} - 1 \right] \Delta T_{\text{axial}}$$

This is equation (11) of the text.

Using results obtained from 2-D calculations for radial power generation in the split-conical core (Figure 4), several problems related to the radial power distribution were investigated. Within a given subassembly in the outer fuel zone, the ratio for $\frac{\text{Peak Power Gen}}{\text{Avg Power Gen}}$ can be as great as 1.17. The central zone has less variation within a given subassembly. If each subassembly heating rate is averaged, the radial variation across the entire core is as follows:

$$\frac{\text{Peak Subassembly Heat Rate}}{\text{Avg Subassembly Heat Rate}} = 1.19$$

$$\frac{\text{Minimum Subassembly Heat Rate}}{\text{Average Subassembly Heat Rate}} = 0.813$$

Since the power distribution depends somewhat upon control rod positions, the range in power generation for the subassemblies should be about 0.80 to 1.20 of the average (radial) subassembly.*

With no orificing in the core and an average core AT of 350 °F, longitudinal subassembly AT would range from 280 to 420 °F, or a total variation of 140 °F.

For the highest radial gradients, concentrate on the hottest duct in the outer fuel zone . . . located at a core radius of about 50 cm. Corresponding members are now

$$\frac{\text{Highest Avg. Power Element, Outer Zone}}{\text{Avg. for Core}} = 1.137$$

$$\text{Peak-to-Average Generation Rate in Subassembly} = 1.17$$

For no mixing, uniform flow, uniform fuel distribution in the hottest outer zone duct,

$$\begin{aligned}\Delta T_r \Big|_{\text{outlet}} &= 2 \left[\frac{P}{A} - 1 \right] \Delta T_{\text{axial}} \\ &= 2 \left[1.17 - 1 \right] \left[1.137(350) \right] = 135 \text{ °F.}\end{aligned}$$

The axial AT is just

$$\Delta T_{\text{axial}} = 1.137 (350) \approx 400 \text{ °F.}$$

The results are shown as the dotted line in Figure 1.

Work by A. Padilla and G. Hesson with COBRA mixing code gives the following flow ratios (based on wetted perimeters and a mixing parameter):

$$\frac{\text{Outer Channel}}{\text{Avg. Channel}} = 1.064$$

$$\frac{\text{Center Channel}}{\text{Avg. Channel}} = 0.983$$

*To provide a flow distribution that will balance the power profile, the flow must be increased by 20% in the hottest channel, thus the core pressure drop would be increased by (approximately) $(1.2)^2$ or 1.44.

HOTTEST SUBASSEMBLY IN OUTER FUEL ZONE
 2-ZONE FTR WITH 350 °F CORE AT,
 NO ORIFICING OF FLOW
 WIRE-WRAPPED PINS, 30 MIL WIRE, 0.25 in. OD PINS
 OUTLET OF FISSION GAS PLENLK

FIGURE D.1. *Approximate Outlet Temperature*

The dashed line of Figure 1 shows the effect of nonuniform flow--but with no mixing. The diametral gap of the element is assumed to be uniformly distributed across the duct--similarly to individually-wire-wrapped pins.

Use of heat balance methods (and assuming that mixing occurs in only the three outer channels) leads to the final solid line curve on Figure 1. A later calculation by A. Padilla evaluated this assumption. His work is shown by the second figure in this appendix. After the transition region, the flow area is increased by about 50 per cent. Because of the small wetted perimeter values in the edge channels, about 2/3 of the flow now flows in the edge channels. With this mixing ratio, the pin bundle outlet temperatures can now be estimated as the arrows of Figure 1. The following figure (in this Appendix) also gives a cross-sectional estimate of temperatures. The results of this method of determining temperature profiles were later confirmed by A. Padilla using computer codes.

The analysis used the following FIR parameter values:

- 400 MW reactor power
- 350 °F core average AT
- 217 pins per subassembly
- 0.25 in. OD pins
- 0.030 in. OD wire wrap
- 4.2 in. subassembly inside dimension (flat to flat)

For elements in the center portion of the reactor, the profile would be more symmetric about the subassembly center (i.e., flatter power profile). However, the large edge gradients would remain. For other elements in the outer fuel zone, only the bulk average AT would decrease: profiles would be similar in shape.

FIGURE D. 2. *Effect of Transition Point from Hexagonal to Circular Duct Shape on Coolant Temperature Distribution at End of Fin Bundle*

Initial considerations of these temperature profiles indicate that:

- The measuring of subassembly average outlet temperatures will require a very effective mixing device.
- The flow maldistribution increases the peak temperatures in a subassembly.
- The pins near the shroud tube wall will be subjected to temperature gradients of approximately 100 °F across one pin.
- The temperature gradient across the shroud tube will remain at the same value as at the core outlet, for the entire length of the gas plenum.

The latter point is most important to the thermal camber problem since it defines the equivalent moment in the subassembly shroud tube.

To make the temperature profiles more closely resemble the power profile, the following suggestions have been proposed.

- Scallop the shroud tube walls
- Use triangular wires running axially along the edge channels
- Use round wires running axially along the edge channels
- Use extra wire wraps around entire pin bundle.

The latter two methods look most promising.

APPENDIX E
CORE AXIAL SUPPORT EFFECTS

APPENDIX E

CORE AXIAL SUPPORT EFFECTS

In top or bottom axial support, the relative motion of the unrestrained end of the subassembly is the same, i.e., about 0.8 in. for a core AT of 350 °F. This is because the portion of the subassembly is being heated while the top cover, bottom tubesheet, and the connecting support structure stay at the same temperatures. With bottom support (Figure 4) the distance from the core centerline to the lower tubesheet changes only a small amount. The entire duct expands 0.8 in., and this motion is taken by a series of springs at the top cover (at 350 °F). A disadvantage is having the core in compression for certain operating conditions such as shutdown flow.

With top plate support, the core centerline moves downward almost 0.8 in. with respect to the top cover tubesheet. Recent information* on density-change reactivity effects for the core is as follows:

$$\begin{aligned} \text{The radial } 6k \text{ per } 1\% \frac{\delta V}{V} &= -0.0032, \\ \text{and the axial } 6k \text{ per } 1\% \frac{\delta V}{V} &= -0.0022. \end{aligned}$$

Downward motion of the core introduces two reactivity components, one radial and slightly positive (0.65 cents) induced by the convergence of core geometry; and the second, axial and negative. Using the above coefficients it was estimated that the cold to hot reactivity change was a net -5¢ if fuel cladding controls the expansion. In the initial startup with "green" fuel, the value would be around 19¢.

* L. Maas's data.

APPENDIX F

EXAMPLE REACTIVITY CALCULATIONS FOR FTR

APPENDIX F
EXAMPLE REACTIVITY CALCULATIONS FOR FTR

Assume split-conical, 2-zone FTR core with following data:

Core AT - 350 °F

Total power - 400 MW

Driver fuel volume - 1000 liters

Critical mass of ^{239}Pu - 650 kg

Power profile at mid-core - Figure 4; P'curve -
 Figure 29

Radial temperature profile, subassembly at 50 cm -
 Figure 1

Axial ΔT_r profile, subassembly at 50 cm - Figure 35,
 curve B

Fuel deflection profile, subassembly at 50 cm -
 Figure 41, Δ curve

Fuel reactivity worth profile, subassembly at 50 cm - Figure 10

From equation (13)

$$R(r) = K_1 \left(\frac{P}{\rho_F} \right)^2$$

at $r = 50$ cm

$$\left. \begin{array}{l} P = 0.67 \text{ MW/liter} \\ R = 14 \times 10^{-4} \text{ } \delta k/\text{kg } ^{239}\text{Pu} \end{array} \right\} \text{ therefore } K_1 = \frac{14(4) \times 10^{-4}}{(0.67)^2} = 0.0125$$

$\rho_F = 2$ (outer zone)

$$\underline{\underline{R(r) = 0.0125 \left(\frac{P}{\rho_F} \right)^2}}$$

From equation (14)

$$\underline{\underline{\frac{\Delta R}{\delta r} \left(\delta k/\text{kg } ^{239}\text{Pu} - \text{cm} \right) = 0.025 P'(r) P(r) \left(\frac{1}{\rho_f} \right)^2}}$$

From equation (15)

$$\delta r = K_3 \Delta T_{\text{axial}} P'(r)$$

$$\text{at } r = 50 \text{ cm, } \Delta T_r = 100 \text{ }^\circ\text{F, } \Delta T_{\text{axial}} = 400 \text{ }^\circ\text{F} \quad (\text{Figure 6})$$

$$\delta r_{\text{avg. in core}} = 32 \text{ mils } 0.032(2.54) = 0.0813 \text{ cm}$$

$$P' = 0.0125 \text{ MW/liter-cm}$$

$$\text{Therefore } K_3 = \frac{0.0813}{0.0125 \cdot 400} = 0.01626$$

$$\underline{\underline{\delta r = 0.01626 \Delta T_{\text{axial}} P'(r)}}$$

$$\text{If orificed core, } \underline{\underline{\Delta T_{\text{axial}} = 350 \text{ }^\circ\text{F}}}$$

$$\text{If uniform flow core, } \Delta T_{\text{axial}} = K_5 P(r) \quad \text{at } r = 50 \text{ cm}$$

$$\underline{\underline{\Delta T_{\text{axial}} = 597 P(r)}} \quad K_5 = \frac{400}{0.67} = 597$$

Combining equations for the two types of cores

$$\Delta R = \left[0.025 \frac{P'(r) P(r)}{\rho_F^2} \right] \left[0.01626 P'(r) \right] \left\{ \begin{matrix} 597 P(r) \\ 350 \end{matrix} \right\} \quad \text{or}$$

$$\Delta R = \begin{cases} P^2 P'^2 \left[0.242 \right] \left(\frac{1}{\rho_F} \right)^2 & \text{Uniform Flow} \\ P P'^2 \left[0.142 \right] \left(\frac{1}{\rho_F} \right)^2 & \text{Flow Orificed} \end{cases}$$

where $\Delta R = \delta k/\text{kg of } ^{239}\text{Pu}$

$$P'(r) = \text{MW/liter - cm}$$

$$P(r) = \text{MW/liter}$$

$$\rho_F = \begin{cases} 1 & r < 50 \text{ cm} \\ 2 & r > 50 \text{ cm} \end{cases}$$

Fuel density in center core region is

$$\frac{1/3 (650 \text{ kg})}{500 \text{ } \ell} = 0.433 \text{ kg Pu/} \ell = 0.433 \times 10^{-3} \text{ kg/cm}^3$$

This value must be used in equation (20) to use a volume integral.

$$\overline{\Delta R} = \frac{\delta k}{\text{cm}^3} = \begin{cases} \frac{P^2 P^{-2}}{\rho_F} [1.05] \times 10^{-4} & \text{Uniform} \\ \frac{P P^{-2}}{\rho_F} [0.615] \times 10^{-4} & \text{Orificed} \end{cases}$$

To evaluate the volume integral the analyst must subtract the closed loop alley and must evaluate separate integrals for differing P , P , ρ_F values. That is, do a summation rather than an integral.

For the closed loop alley, (See Figure 8) convert constant from π to

$$K = \begin{cases} 2.66 & \text{Center Region } 0 \leq r \leq 48 \text{ cm} \\ 2.9 & \text{Outer } 48 \leq r \leq 66.5 \text{ cm} \end{cases}$$

This gives a volume of 500 liters per region [$h = 32(2.54) = 81.3 \text{ cm}$].

The following table evaluates the volume integral for total core reactivity change. Note (last column) that the contributions to reactivity are concentrated in the outside tube rows.

TABLE F. I. $FTR \text{ Reactivity Integral} \equiv 2Kh \int \overline{\Delta R} r dr$
 $= 2Kh \sum_i \overline{\Delta R}_i \Delta r_i r_i$

i	2kh	Δr_i	r_i	ρ_F	$P(r_i)$	$P'(r_i) \times 10^3$	$\frac{10^{10} \times \overline{\Delta R}_i}{(\text{uniform, } \times \text{ orificed})}$	$10^{-2}(2Kh)$
								$Ar_1 \times r_1 \times \overline{\Delta R}_i$
1	432	12	6	1	0.62	0	(0, 0)	(0, 0)
2	432	12	20	1	0.643	(-3.5)	(-5.31, -4.77)	(-0.0055, -0.0049)
3	432	12	32	1	0.713	1.7	(1.54, 1.25)	(0.0026, 0.0021)
4	432	12	43	1	0.615	5.8	(13.36, 12.5)	(0.0298, 0.028)
5	472	6	51	2	0.66	12.5	(15.4 , 15.85)	(0.0223, 0.0229)
6	472	6	57	2	0.575	14.4	(17.9 , 18.3)	(0.0229, 0.0296)
7	472	6 1/2	64	2	0.455	18.0	(17.6 , 22.7)	(0.0346, 0.0447)

Total in % δk (0.113, 0.123)

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