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AEC RESEARCH AND
DEVELOPMENT REPORT
JUNE 1970

MASTER

DESIGN AND ANALYSIS OF SEFOR CORE 1

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U.S. ATOMIC ENERGY COMMISSION
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DESIGN AND ANALYSIS OF SEFOR CORE I

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ABSTRACT

The thermal-hydraulic, mechanical and neutronic analysis and design of SEFOR Core I is described. Both the static and transient aspects of the reactor operation are considered in the analysis of fuel rod and core structure behavior. Design details of the fuel rod and core hardware are presented together with a mechanical design evaluation.

INTRODUCTION

This report describes the design and analysis of the SEFOR Core I, including the thermal-hydraulic, mechanical, and neutronic design.

The Southwest Experimental Fast Oxide Reactor (SEFOR) is a 20 MW(t) fast spectrum reactor fueled with mixed $\text{PuO}_2\text{-UO}_2$ and cooled with sodium. SEFOR will have characteristics similar to large, soft spectrum fast breeder reactors fueled with mixed $\text{PuO}_2\text{-UO}_2$. Economic studies of these large power reactors indicate significant potential for producing low cost power. SEFOR will be used to obtain physics and engineering data at fuel compositions, temperature, and crystalline states characteristic of power reactor operating conditions. SEFOR is particularly designed for the systematic determination of the Doppler coefficient of reactivity at temperatures up to the vicinity of fuel melting.

The SEFOR Project consists of two major parts: the design and construction of the reactor and a related research and development program. Funds for the design and construction of the facility are being provided by the Southwest Atomic Energy Associates (a group of seventeen investor-owned utility companies located in the South and Southwest part of the United States), together with Karlsruhe Laboratory of the Federal Republic of Germany, Euratom, and the General Electric Company.

The United States Atomic Energy Commission is supporting the research and development program. The R and D Program consists of two phases:

Phase I - Pre-operational Research and Development

Phase II - Post Construction Research and Development

This report describes the work performed as a part of Task 8, Core Design in Phase I of the Research and Development Program.

Task 8 encompasses design of the reactor core, including nuclear physics, thermal-hydraulic considerations and structural analysis. In addition to the reactor core (fuel rods and core structure), reactivity control requirements are included in Task 8. The scope of work covers conceptual design, detail design and finished engineering drawings.

SECTION I

1.0 FUEL MECHANICAL AND THERMAL ANALYSIS1.1 CORE THERMAL-HYDRAULIC DESIGN1.1.1 Main Coolant System Hydraulics

Figure I-1 and I-2 show the SEFOR main coolant systems and details of the reactor vessel flow paths. The design coolant flow rate through the SEFOR core at 20 MW_t steady state operating conditions is 518 lbs/sec. This corresponds to 4360 gpm at the core average coolant temperature of 760°F and results in a mixed average core coolant temperature rise of 120°F . Table I-1 gives the primary system flow balance for the rated core flow of 4360 gpm. The leakage within the reactor vessel has been broken down into various contributions. These leakage values represent a maximum value based upon dimensional tolerance extremes and upon measurements that were made in the 1/2-scale vessel test⁽¹⁾ and in the SEFOR prototype fuel bundle water tests.⁽²⁾ Table I-1 gives the total vessel flow as 5019 gpm of which 4784 gpm is supplied by the main primary pump. Also shown on the table is the predicted pressure drop for the primary system of 28.9 psi. Since the main primary pump is rated at 38 psi developed head at 5000 gpm, Table I-1 demonstrates that adequate margin is available to get the desired core coolant flow at 20 MW_t conditions.

Figure I-1. SEFOR Cooling System

TABLE I-2

REACTOR VESSEL AND EXTERNAL SYSTEM PRESSURE LOSSES

	Pressure Drop (psi)
Inlet Nozzle to Lower Plenum	4.9
Reactor Core	3.8
Downcomer Channels	0.2
Exit Annulus to Exit Nozzle	6.5
Total Vessel Pressure Loss	15.5
Piping and Flowmeter	9.9
IHX	3.5
Total System Pressure Loss	28.9

A core element pressure drop of 4 psi combined with the buoyancy force produces a total of 5.7 pounds hydrodynamic loading (buoyancy plus viscous drag) on the rods at the center of the core. (Rods in channels with maximum coolant flow) Rods away from the core center region have less loading because a larger part of the 4 psi core pressure drop is taken across the orifice which is attached to the grid plate. The total weight of a fuel element (fuel rod and attached extension rod) is 15 pounds which gives approximately a factor of three margin for gravity hold-down of the fuel rods against the upward hydrodynamic loading. The total weight of the B_4C rods is somewhat less than that of the fuel rods but still more than twice the upward force exerted on these rods. In Table I-3 the weights and maximum hydrodynamic loadings on the fuel rods and special rods in the core are summarized. As can be seen in Table I-3, the minimum difference between element weight and upward forces is 5.3 pounds for the BeO tightener rod. This margin covers any uncertainty in core pressure drop or any accidental increase in flow above rated flow conditions up to and including the maximum flow capability of the primary pumps.

TABLE I-3CORE ELEMENT WEIGHTS AND HYDRODYNAMIC LOADINGS

<u>Core Element*</u>	<u>Maximum Upward Force (lb)</u>	<u>Element Weight (lb)</u>
Fuel Rod	5.7	15.0
UO ₂ Rod	5.7	17.0
Stainless Steel Rod	5.7	15.0
B ₄ C Poison Rod	5.7	11.9
BeO Tightener Rod	4.7	10.0
Foil Holder Rods	2.1	8.6

* Foil holder rods will be used only during wet critical testing.

As mentioned above, the hydrodynamic loadings given in Table I-3 include the viscous drag force. The loading on individual rods was calculated by multiplying the total drag force in the bundle (which is the coolant flow area multiplied by the pressure drop across the core) by the wetted perimeter of the individual rod divided by the total wetted perimeter in the fuel channel. If the fuel channel should become completely blocked by material attached only to the fuel rod (worst case condition), the total upward force on the fuel rod would only increase about 0.25 lb to approximately 6 pounds. A more meaningful comparison for this extreme case is the margin between the weight of the moveable parts (fuel rods, tightener rod and tightener sleeve) inside the fuel channel (~110 lbs) and the total upward force of 61 pounds that would be exerted on the moveable parts of the hot channel for the blocked channel condition. This factor of about two assures that fuel rods will not be forced out of the core even for this extreme condition.

Figure I-2

SEFOR Reactor Vessel

TABLE I-1

<u>PRIMARY SYSTEM FLOW BALANCE FOR CORE FLOW OF 4360 gpm</u>		
Core Flow	4360 gpm	100%
Leakage Flow	659 gpm	15%
Seal Ring	108 gpm	2.47%
Siphon Breaker	221 gpm	5.07%
Bellows Assemblies	264 gpm	6.05%
Main Outlet Nozzle Sleeve	56 gpm	1.28%
Auxiliary Inlet Nozzle Sleeve	10 gpm	0.23%
Vessel Flow	5019 gpm	
Main Axuiliary Pump	235 gpm	
Main Primary Pump	4784 gpm	
Piping and Flowmeter ΔP	9.9 psi	
IHX	3.5 psi	
Vessel	<u>15.5 psi</u>	
	28.9 psi	

Table I-2 shows the detailed breakdown of the pressure drop through the reactor vessel. The individual pressure losses in the vessel were measured in the 1/2-scale vessel model test, the results of which were summarized in Reference 1. The pressure drop through the fuel channel has been measured in more detail in prototype fuel bundle water tests⁽²⁾ and in the prototype fuel bundle sodium tests.⁽³⁾ The overall fuel bundle pressure drop as measured in the water tests and sodium tests compared quite well with each other and with the analytic predictions made using smooth tube friction factors.

1.1.2 Core Orificing

A thermal-hydraulic design objective for the core is to match power and flow in each of the fuel channels, to give a uniform core coolant temperature rise. The orifice zones are shown in Figure I-3 and the relative flow per channel at design conditions is shown in Figure I-4. The flow distribution is based on results for the 1/2-scale vessel flow test⁽¹⁾ and standard fluid flow calculations. Loss coefficients used in these calculations were measured in the prototype fuel bundle water tests.⁽²⁾ Table I-4 is a summary of these calculations for three orifice positions.

Thermal calculations have been performed using the orifice zones and the flow distributions shown on Figures I-3 and I-4, respectively. These calculations assumed no mixing within the individual fuel channel. As discussed later in this section, the assumption of no mixing within the channel is not ultra-conservative for the SEFOR design. Without mixing, coolant flowing on the side of the channel nearest the center of the core has a higher temperature rise (due to the higher power density nearer the core center as shown on Figure I-5) than the sodium flowing on the side of the channel away from the center of the core. Figure I-6 shows the results of these calculations. On this figure it is seen that the core temperature rise varies from minimum of approximately 100°F to a maximum of about 145°F. It should be noted that the temperature rise in the center channel is larger than the nominal 120°F. This reflects the use of the hot channel flow factor which corresponds to about 15% lower flow in the central channel than that indicated by the predicted relative flow distribution shown in Figure I-4. It should be noted that the radial temperature distributions shown on Figure I-6 across an individual channel will result in bowing gradients. The effect of these bowing gradients on core dimensions is discussed later in this supplement in Section 1.4.

TABLE I-4

FUEL CHANNEL PRESSURE LOSSES

Orifice Zone	A _o	B ₂	F ₈
Core Flow Rate, lb/sec	6.91	5.90	3.27
Plenum Velocity Head Loss, psi	0	0.32	0.95
Orifice	0.65	1.10	2.14
Fuel Zone Spacers	0.86	0.63	0.20
Fuel Zone Rod Friction	1.41	1.07	0.38
Cross Flow Section	0.28	0.20	0.06
Core Coolant Flow Rate, ft/sec	8.10	6.91	3.83
Ext. Rod Flow Rate, lb/sec	6.01	5.34	1.34
Ext. Rod Spacer Losses, psi	0.16	0.12	0.006
Ext. Rod Friction	0.27	0.22	0.02
Exit Losses	0.13	0.10	0.004
Total Pressure Loss	3.76	3.76	3.76

NOTE: - Letters refer to channel positions as shown on Fig. I-3.

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Figure I-3. SEFOR CORE, ONE-THIRD SECTOR, SHOWING ORIFICE ZONES

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Figure I-4. RELATIVE FLOW PER CHANNEL COMPARED TO THE
 RELATIVE POWER DISTRIBUTION (CORE DESIGN FLOW)

Figure I-5. RADIAL DISTRIBUTION OF POWER DENSITY

Figure I-6. Core Coolant Temperature Rise (No Mixing in Channel)

1.1.3 Local Coolant Flow Distribution

In addition to the gross coolant temperature variations (due to the radial power profile and the assumptions of no coolant mixing with the channel), significant local temperature variations exist within the individual fuel channels due to mass flow variations around the individual fuel rods. This effect is discussed in Reference 1 with a summary of the experimental results which verified the analytic model that was used to predict mass flow variations for the thermal calculations. Reference 4 reports the three scale water tests that were used to verify the analytic predictions. The major conclusions from this experimental work are:

- (1) The mass flow variation around the fuel rod varies as a product of velocity and flow area so that the local flow rate is strongly dependent upon local flow area.
- (2) The values of mass flow variation obtained by the analytical model (see Reference 4 for summary of the analytic model) agree well with test data and may be used for analysis of flow distributions in areas of irregular cross section.

Based on these conclusions, mass flow variations around an individual fuel rod were calculated. Figure I-7 shows a cross section of a 30° segment of a fuel channel. This section is typical for the nominal case because of symmetry. Shown on the figure are the nine 20 degree circumferential nodes that were used for the slug flow calculation performed to obtain the circumferential mass flow variations. Reference 4 contains a summary of the slug flow equations used to determine the mass flow in each of the nine coolant

Figure I-7. Typical Section of SEFOR Fuel Channel

nodes. Figure I-8 is a plot of the results from this calculation. This figure shows the relative mass flow distribution (normalized) as a function of the angular position around the fuel rod. Figure I-7 shows the location of the 0-180 degree axis. From Figure I-8 it can be seen that the local flow varies from +72% to -45% of the desired uniform flow condition. The dashed curve shows the discrete values calculated for the nine circumferential node positions. These discrete values were used in the heat transfer calculations described in Section 1.2. The solid curve represents the actual circumferential mass flow distribution expected around the rod and were drawn by requiring that the area under the solid curve equal the area under the dashed curve in each of the nine nodes. Mass flow variation calculations similar to the one described above for the nominal rod position have been repeated for rod positions corresponding to dimensional tolerance extremes. The results from these calculations (discussed later in this section) were used in the heat transfer calculations which are described in the following section.

Figure I-9 is a typical cross section of the SEFOR fuel channel which shows the support system used to position the fuel rod within the fuel channel. Shown on the figure are the spacer buttons, fins, springs, and dimples inside the fuel channel and the dimensional tolerances on these parts. Also shown on the figure are the nominal spacing at the three point where the fuel rods come tangent to cold or heated surfaces. For convenience, these critical points have been labeled ΔX_1 , ΔX_2 , and ΔX_3 to identify the spacing between the fuel rod and the tightener sleeve, between the fuel rod and the channel, and between adjacent fuel rods, respectively.

By comparing the nominal spacings and dimensional tolerances shown on Figure I-9, it can be concluded that the fuel rod cannot be moved out toward the channel wall more than approximately 3 mils from the nominal position. This has a very small effect on flow distribution and for all practical purposes corresponds to the nominal case. However, the rod can be moved in the direction toward the tightener sleeve from the nominal 93 mils to a minimum (with the springs compressed), corresponding to the minimum-sized dimple in the tightener sleeve of 55 mils (nominal is 60 mils). A fuel rod can also be moved from its nominal position toward another fuel rod. In this case, the spacings between rods can be decreased from the nominal of

Figure I-8. Coolant Mass Flow Distribution (Nominal Fuel Rod Position)

Figure I-9. SEFOR Fuel Rod Support System

125 mils to a minimum corresponding to the minimum height of the dimples in the fins of 65 mils (nominal is 70 mils). Because these two positions of the rod represent extreme conditions, mass flow calculations have been done at these positions for use in bounding-type heat transfer and stress calculations. Results from these calculations are shown on Figures I-10 and I-11. Note that for the case of a rod moving toward another fuel rod (Figure I-11) symmetry is lost so that calculation is done for the entire (360 degree) rod.

Mass flow distributions for the dimensional tolerance extreme cases were calculated assuming the rod moved in the vertical position, either in the direction toward the tightener sleeve or in the direction toward an adjacent fuel rod. In the actual case, the temperature variation due to the mass flow variations will result in bowing gradients which will tend to make the fuel rod deflect from the straight condition. The bowing calculations performed are discussed in Section 1.5 of this supplement. The results of these calculations indicate that bowing of the rod tends to cause coolant mixing and redistribution so that the assumption of the rod moving in the vertical position and no flow mixing represents the conservative bound on the mass flow variations which occur within the core.

1.2 STEADY STATE TEMPERATURE DISTRIBUTIONS WITHIN A SEFOR FUEL CHANNEL

1.2.1 Introduction

A series of heat transfer calculations were performed for rods at various positions in the core. Particular attention was given to the rod in the hot channel and in the average position in the core. Typical results of these calculations are discussed in this subsection. The cases presented in the subsection are limited to the 20 MW operating condition since this operating condition results in the maximum temperature variations within the fuel channel. In addition, only results from the three rod positions discussed above (nominal and the two dimensional tolerance extremes) will be included.

Conservative values for the channel coolant flow and power density were used in all the calculations. A summary of these factors is given in Table I-5 below for the cases to be presented. The columns labeled "Predicted" contain the calculated values based on the power peaking

Figure I-10. Coolant Mass Flow Distribution (ΔX_1 Minimum)

Figure I-11. Coolant Mass Flow Distribution (ΔX_3 Minimum)

factors and flow peaking factors presented in Section III and 1.1.2 of this supplement. It should be noted that the hot rod calculations were performed using a power density 3% higher than predicted, and a flow 10% lower than predicted. These margins cover the estimated uncertainties expected in predicting the local power densities and local coolant flow distributions in the SEFOR core. A discussion of uncertainties in the power peaking factors is given in Section III of this supplement. The average channel calculations were done with the expected values since this calculation is used to represent an average position in the SEFOR core. The flow factor is also adequate to cover the reduced nominal coolant flow past "guinea pig" rods (discussed in Section 1.6) operating at the normal peak rod power densities.

TABLE I-5
POWER AND FLOW PEAKING FACTORS

<u>Location</u>	<u>Power Peaking Factors</u>		<u>Flow Peaking Factors</u>	
	Predicted	Used in Calc.	Predicted	Used in Calc.
Hot Rod	1.485	1.53	1.55	1.41
Average Rod	1.0	1.0	1.0	1.0

1.2.2 Steady State Fuel Rod Heat Transfer Calculations

Steady state heat transfer calculations have been performed on the SEFOR fuel channel using a general heat transfer code. This code has the capability of solving heat transfer equations in either rectangular or cylindrical geometry using a nodal technique. Reference 5 is a description of the TIGER II Heat Transfer Code. The generalized heat transfer code used for the core analysis is an improved version of the TIGER II. The improved version uses the same numerical technique and solves the same heat transfer equations as TIGER II.

A cross section view of the basic unit cell that was used in the local heat transfer calculations is shown on Figure I-12. Figure I-13 shows a simplified isometric view of the heat transfer model used to analyze the local coolant effects. For convenience, this sketch is shown in rectangular geometry; however, the nodal system was set up in a cylindrical coordinate system for this calculation. As can be seen from

Figure I-12. SEFOR Fuel Rod at Axial Position 6
(Heat Transfer Model)

Figure I-13. SEFOR Local Coolant & Cladding Heat Transfer Model

Figure I-13, the fuel rod has been divided up into seven axial nodes with each axial node having nine circumferential nodes. It should be noted that there are no fuel nodes in this model. Instead, the basic assumption is made that the energy input to the ID of the cladding is uniform circumferentially. This assumption does not introduce appreciable error since resistance to circumferential heat transfer within the fuel meat is considerably more than in the cladding or coolant. Further, the assumption is conservative for these calculations since any circumferential heat transfer that does occur in the fuel meat will tend to decrease the temperatures and temperature variations in the cladding and the coolant. The effect of this assumption is that the grid on the cladding and in the coolant can be made finer thus giving a better heat transfer calculation in the cladding and coolant. Axial heat generation variations are included in the calculations. The axial variation is calculated using a chopped cosine power distribution for the SEFOR core. An average power density was determined for each of the seven equal length fuel nodes from this distribution. It will be noted on Figure I-13 that the segmenting gap is not included in these calculations. This gap, which is located near the bottom of the fifth axial node, has been treated separately in heat transfer calculations. Results from these calculations have indicated that removing the gap and assuming that the active fuel is continuous, gives accurate predictions of the temperature distribution in the cladding and the coolant along the full length of the active fuel region.

The heat transfer calculations assume circumferential heat transfer in the cladding (no axial heat transfer). Referring to Figure I-13, nodes 1 and 2 are coupled by conductive heat transfer, but 1 and 10 are not. Each of the cladding nodes in turn are coupled to a coolant node by conductive heat transfer in the cladding and by an effective cladding to coolant heat transfer from the surface of the cladding to the coolant. Thus, for example, node 1 in the cladding is coupled by conductive heat transfer to the clad surface (designated as 64) and then in turn by an effective clad-to-coolant heat transfer coefficient to the coolant node 127. The effective clad-to-coolant heat transfer coefficient is calculated assuming molecular conduction only, with no contribution from

eddy heat transport. These coefficients vary circumferentially and also as a function of fuel rod position in the channel which changes the characteristic width of the local coolant node. Table I-6 gives an example of the clad-to-coolant heat transfer coefficients used (for the nominal fuel rod position).

Axial coupling in the heat transfer calculation does occur in the coolant nodes. In turn, each of the nine coolant regions (or flow tubes) are coupled circumferentially to adjacent coolant nodes. Again, referring to Figure I-13, coolant node 181 is coupled through an effective heat transfer coefficient labeled 349 to coolant node 182.

TABLE I-6
LOCAL CLAD-TO-COOLANT HEAT TRANSFER COEFFICIENTS

<u>Location¹</u>	<u>h_f (BTU/Hr-Ft²-°F)</u>
172	8700
173	7000
174	12,300
175	12,300
176	8300
177	5300
178	8000
179	11,400
180	8000

1 -Node numbers refer to coolant nodes in Figure I-12.

This effective coefficient is again based on molecular conduction only and is determined by considering the distance from the centroid of coolant node 181 to that of 182 and determining an average heat transfer area through which the heat is transferred circumferentially.

The basic model described above has been used to do a series of steady state heat transfer calculations. In addition, this model has been used to perform transient calculations as discussed in Section 1.3. Results obtained from this model include fuel cladding temperature at nine circumferential positions for each of the 7 axial positions

and the coolant temperature at nine circumferential positions for each of the seven axial positions. These results provide the basic input for determining fuel rod bowing gradients and local stresses as well as a prediction of the maximum temperatures in both the cladding and the coolant. Figure I-14 is an example of the type of information obtained by these calculations. This figure shows circumferential temperature variations of the coolant at the core outlet around one-half of the rod. The distribution shown is for the rod in the nominal position and at the average channel position in the core. Figure I-15 is a plot of the same type of information but for the rod in the hot channel position in the core. Note that the peak coolant outlet temperature obtained for this hot rod calculation is approximately 875°F. This temperature presents no special problems for the steady state 20 MW_t operating conditions. The significance of these local high temperature regions for the transients is discussed in Section 1.3.

The circumferential coolant temperature variations shown on Figures I-14 & I-15 result in a corresponding circumferential temperature variation in the fuel cladding. These temperature variations are of concern because of possible bowing gradients (the linear component of such variations) and the local stress due to the non-linear portion of the temperature distribution. Figure I-16 is a plot of the cladding circumferential temperature distribution for an average fuel rod about seven inches from the top of the active fuel region in the core. This is the location where the maximum variation in clad temperatures occur. Figure I-16 shows two cases for power generation in the average rod. One case assumes a uniform power generation across the fuel rod and the second case (which is the actual situation) which uses the power gradient which exists across the fuel rod at the average position in the core. In the latter case, the change in power density across the rod is approximately 10%. The significance of this comparison is that the rod bowing gradient is not a strong function of position in the core (relative to the core center) or to core power level.

Figure I-14. Core Coolant Outlet Temperature Distribution (Average Rod)

Figure I-15. Core Coolant Outlet Temperature Distribution (Hot Rod)

Figure I-16. Fuel Cladding Temperature Variation (Average Rod)

Figure I-17 is a plot showing the linear portion of the circumferential temperature variation (bowing gradient) which will tend to make the rod deflect from its straight condition. The significance of these bowing gradients is discussed in Section 1.5.

Figure I-18 is a plot of the circumferential temperature variations in the cladding of the hot rod at 20 MW conditions comparable to the average rod results shown in Figure I-17. Plotted on Figure I-18 is the temperature distribution for the seven axial positions used in the heat transfer calculation. From this figure it can be seen how the local temperature variations build up as the coolant goes up through the core reaching a maximum at about the fifth axial position in the core (which is approximately 7" from the top of the fuel). Figure I-19 shows how the linear component (bowing gradient) builds up from the bottom to the top of the active fuel region. The response of the straight fuel rod to the bowing gradient shown on Figure I-19 has been analyzed treating the rod as a beam with support points corresponding to spacer positions in the fuel channels and with the bowing gradient shown on Figure I-19. Figure I-20 shows a typical deflection mode of the centerline of the fuel rod and indicates the contact points within the core. (See further discussion in Section 1.5). Note that the top of the fuel rod has moved in toward the tightener sleeve compressing the leaf spring which is located at the top of the core. This spring is compressed until the fuel rod touches one of the dimples in the tightener sleeve since the spring constant of these springs is not large enough to resist the bowing forces applied by the deflection of the fuel rod. With the rod in this position, it is supported by a metal-to-metal contact at three elevations in the core and a further increase in the bowing gradient either during a transient or due to the flow perturbations introduced by this bowing mode, will not result in the further deflection of the fuel rod.

As mentioned above, the bowing gradient calculated for the straight rod will be perturbed when the rod deflects under the influence of the bowing gradient because of the perturbation of the coolant mass flow distribution. As shown in Figure I-20, this will be particularly significant near the top of the core where the local flow area will be significantly less than that which would exist with the rod in its nominal

Figure I-17. Cladding Temperature Distribution and Thermal Bowing Gradient (Average Rod)

Figure I-18. Fuel Clad Temperature Distribution (Hot Rod)

Figure I-19. Fuel Rod Bowing Gradient (Hot Rod)

Figure I-20. Typical Fuel Rod Bowing Mode

position. Since the rod is supported by metal-to-metal contact in this position, an increase or decrease in the bowing gradient will not result in a significant change in the deflection of the fuel rod. However, changes in the bowing gradient do affect the bending stresses. Calculations were done to evaluate this feedback effect on the bowing gradient to obtain the bending stresses given in Section 1.5.

The mass flow variation as a function of axial position due to the fuel rod motion can be calculated using a local hydraulic diameter concept as was demonstrated in the three-scale model tests, the summary of which appears in Reference 4. However, as the mass flow variation changes as you move axially through the core, mixing takes place which is difficult to account for in the thermal calculations. Therefore, a conservative bounding type model was assumed to assess the magnitude of the bowing gradients and hence the bending stresses and local stresses that could result for rods displaced from their nominal position. This model (described in Section 1.1.3) assumes that the fuel rod axis remains vertical. This model is clearly conservative since in the actual case, the fuel rod will be bowing away from the tightener sleeve at one axial position and toward the tightener sleeve in another axial position (as can be seen on Figure I-20 for a typical case). Dimensional tolerances have been included in these calculations. Coolant flow distributions for the two most extreme tolerance conditions are shown on Figures I-10 and I-11. The resulting clad temperature distributions for the hot rod are shown on Figure I-21 and I-22 and compared to the nominal position. From Figure I-22 it can be seen that the maximum clad temperature increases from the value of approximately 920°F to approximately 1030°F. These peak temperatures have been used in determining the properties of the material for assessing the structural capability of the cladding. The linear component of the cladding temperature variations for these two positions were calculated. These bowing gradients have been used to calculate the bending stresses with the support conditions in the core as well as the local stresses due to the non-linear portion of the temperature distributions. (See Section 1.5).

Figure I-21. Fuel Cladding Temperature Variation (Hot Rod)

Figure I-22. Fuel Cladding Temperature Distribution (Hot Rod)

The calculations described above are an example of the type of calculations that were performed to determine the temperature distributions and bowing gradients for the fuel rod in the SEFOR reactor. These calculations considered the case of moving the fuel rod along a line from the corner of the channel to the tightener sleeve so that the 180 symmetry was preserved and the case where fuel rods move in a direction parallel to the flats on the tightener sleeve in the direction of an adjacent fuel rod. The latter case results in a three-dimensional deflection mode for the fuel rod. The results from the three-dimensional calculation show that the three sets of fins which are attached to the tightener sleeve and which project out between the individual fuel rods are adequate to maintain a minimum spacing between fuel rods of 0.070". Results of calculations (See Section 1.5) show that this spacing will not cause excessive stress levels or high temperatures in either the cladding or the coolant during steady state or transient operation.

To summarize then, the temperature distribution in the cladding and the coolant of the hot channel at the 20 MW_t operating condition have been determined for the nominal position and for a number of dimensional tolerance extreme positions assuming the fuel rod remains vertical. The cladding temperature distributions have been used to determine the mode of deflection of the fuel rod within the fuel channel. The affect of these deflections on mass distribution have been conservatively calculated and used to determine the maximum fuel cladding and coolant temperatures. These calculations have demonstrated that the fuel rod motion is limited by the spacer buttons, dimples on the tightener sleeves and the fins so that adequate flow is maintained on all surfaces.

The results presented in this section are for 20 MW_t operating condition with the nominal 700° inlet and 120° core temperature rise. As discussed in Reference 6, experiments are planned in which the core inlet temperature is changed from the 700° value and in which the coolant does not match the power. This condition results in outlet temperatures greater than 820 degrees and core ΔT 's greater than 120°F. Figure 4-1 of Reference 6 is a summary of the operating map that was used to evaluate the design of the fuel. Calculations have been

repeated for various positions along the boundary of this operating map and have been considered in the steady state structural analysis in Section 1.5.

1.2.3 Steady State Segmenting Plug Heat Transfer Calculation

The heat transfer calculations discussed in Section 1.2.2 considered only the active fuel portions of the fuel rod. A separate heat transfer model was developed to analyze the segmenting plug region of the fuel rod. The U-shaped segmenting plug is of particular interest because it is the structural member that supports the upper fuel column and because it is separated only about 3/8-inch from the active fuel by the depleted UO_2 insulator pellet. The bottom and top end plugs are separated several inches from heated fuel and see very low loading for steady state operation.

Figure I-23 is a cross section of the heat transfer model used to analyze the temperature distribution in the segmenting plug region. The figure shows the nodal arrangement used. The grid used is considerably finer for the segmenting plug than for washer since the latter is not a structural member. Circumferential symmetry was used for these calculations since the main concern is the peak temperatures in the structural members. This model was set up for internal heat generation in both the fuel and the insulator material. A fast fission heat generation equal to 15% of the power density in the adjacent fuel was used for these calculations. The actual value is calculated to be only 11%. Direct heating in the structural material is small relative to that in the fuel and insulator material and was neglected.

Figure I-24 is a plot of the temperature isotherms in the segmenting plug region of the hot rod with the reactor operating at 20 MW_t . A uniform sink temperature of 775°F was used for these calculations. Note that the peak metal temperature of about 1150°F occurs in the 0.080" washer. The maximum temperature in the segmenting plug itself is about 1050°F . The Inconel-X spring would operate somewhere between these two temperatures. At these temperatures, all of the structural material in the segmenting plug region would have high strength. Similar calculations which have been performed for the Design Transient are discussed in Section 1.3.5.

Figure I-23. Heat Transfer Model for Segmenting Plug Region

Figure I-24. Temperature Isotherms in Segmenting Plug Region (20 MWt)

1.2.4 Steady State Heat Transfer Calculations for Special Rods

A number of special rods will be used in the core during the planned experimental program. These rods have external dimensions very similar to the standard fuel rod and will be inserted in a standard fuel rod position. Two of the most important special rod types are the "guinea pig" rods and the instrumented fuel rods.

The operating power density planned for the "guinea pig" rods (as discussed in Section 1.6) is 100-115% of the normal hot rod power density. In addition, the rods are located in channels away from the center of the core which have somewhat less flow than the center channels. However, because the hot rod calculations use a 10% reduction in flow (flow hot spot factor), the hot rod calculations approach the nominal conditions expected for the "guinea pig" rods. Therefore, the mismatch in power density and flow is only slightly more severe than that used for the hot rod calculations. The main concern with the "guinea pig" rods is the higher fuel temperature due to the higher power density operation planned for part of these rods. This is discussed in detail in Section 1.6.

The instrumented fuel rods have the same power density as normal rods so the calculations performed for the normal rods are directly applicable. All closures and changes in cross sections have been evaluated for steady state and transient operating conditions and are designed to minimize thermal stresses in much the same way as was done for the standard fuel rods.

Other special rods in the core include B_4C filled poison rods, depleted UO_2 rods and stainless steel filled rods. The external dimensions of these rods are identical to that of the standard fuel rods. Since the internal heat generation in these rods is small, the loadings on the tubing and welds in these rods is considerably lower than for the standard fuel rods.

The BeO filled tightener rod also has very low loadings compared to the fuel rods. These rods will operate very near the temperature of the surrounding coolant.

1.3 TRANSIENT ANALYSIS

1.3.1 Introduction

The first step in the transient analysis of the core was to select a Prompt Critical Power Excursion (hereafter referred to as the Design Transient) that would be greater than any of the prompt critical power tests planned for Core I or Core II. This condition is met by selecting a transient which gives energy additions and energy addition rates to the core that are significantly larger than the maximum prompt critical transients planned during the three year test program. Parameter studies of prompt critical transients in SEFOR have shown that the significant parameters in determining the magnitude of the transient are (1) the magnitude of the Doppler coefficient, (2) the rate at which reactivity is added to the core, (3) the magnitude or the total amount of reactivity inserted for the test, and (4) the initial steady state power (fuel temperature) of the core. These parameter studies have shown that the magnitude of the transient increases as (a) the size of the Doppler coefficient is decreased, (b) the rate of reactivity insertion is increased, (c) the magnitude of the total amount of reactivity inserted increases, and (d) the initial steady state operating power (fuel temperature) increases.

1.3.2 Design Transient and Planned Prompt Critical Transients

The dependence of the energy release from the prompt critical transients as summarized above was used to select a Design Transient which would be an upper limit on the total energy released and rate of energy release in the core for the prompt critical testing for both Core I and Core II. Table I-7 summarizes the important parameters that were used in the Design Transient. The magnitudes of these important parameters are also compared to maximum planned transients in Table I-7. The planned transient column in Table I-7 is divided into two headings. The first column is $T \frac{dk}{dT} = -.0085$ and represents the predicted Doppler for the Core I. The second column labeled $T \frac{dk}{dT} = -.004$ represents the lower limit on Doppler magnitude that would be acceptable for the Core II testing and, therefore, represents a lower bound (for the Doppler coefficient) on the planned transient tests that could be conducted in Core II. Comparing the values for the Design Transient and for the planned transients in Table I-7, it can be concluded

TABLE I-7
COMPARISON OF THE DESIGN TRANSIENT TO PLANNED TRANSIENTS

<u>Quantity</u>	<u>Design Transient</u>	<u>Planned Transients</u>	
		$T \frac{dk}{dT} = -.0085$	$T \frac{dk}{dT} = -.004$
Doppler Coefficient ($T \frac{dk}{dT}$)	-0.004	-0.0085	-0.004
Reactivity Inserted	1.5\$	1.3\$	1.2\$
Reactivity Insertion Rate (Max)	50\$/sec	20\$/sec	20\$/sec
Initial Power	7 MW	11.5 MW	9 MW
Core Coolant Flow	100%	100%	100%

that the Design Transient will result in a larger energy release and higher energy release rates (based on the summary of the dependence of energy release on important parameters presented in Section 1.3.1) than during the planned transients. In particular, the design transient uses the minimum Doppler coefficient and a total reactivity and reactivity insertion rate that is significantly larger than that for the planned transients. The initial power for the Design Transient is slightly lower than the values given for the planned transient cases. This slight non-conservatism is completely over-ridden by the larger insertion of reactivity and insertion rates used for the Design Transient. The 7 MW initial power level was chosen since it represents the maximum power at which the design transient could be initiated without resulting in melting of the fuel in the hot rod (a basic criteria for the transient testing program).

1.3.3 Transient Power and Temperature Calculations

The three transients listed in Table I-7 have been analyzed using the FORE I Digital Computer Code. A description of the FORE I code is given in Reference 7. It should be noted that FORE I has been in use for approximately five years both in GE and in other organizations. During this period of time comparisons have been made with other point kinetics codes and a considerable number of widely varying problems have been analyzed. The combination of comparison runs and the wide spread usage of this code gives confidence in the accuracy and correctness of the calculations.

The FORE I model used for this analysis divided the fuel rod into eight radial nodes. The outer fuel node was coupled through the fuel-to-clad heat transfer coefficient to the internal cladding node. The fuel-to-clad heat transfer coefficient used varied from 1000 BTU/hr ft²°F to 4000 BTU/hr ft²°F depending on position in the core and size of the transient. The temperature dependent fuel properties (thermal conductivity and heat capacity) used in the calculations are discussed in Section 1.6.

A summary of results for the FORE I calculations for the design transient are shown on Figures I-25 and I-26. Figure I-25 includes plots of the power pulse as a function of time and the average fuel temperature rise during the excursion. The average fuel temperature rise is the integral of the power trace and represents a convenient measure of the size of the transient. The maximum fuel temperature reached during this transient was 5100°F. This coincides with the melting point of the ceramic fuel which is 5100 ±100°F (see Section 1.6.2.3). Figure I-26 is a plot of the mixed average core outlet temperature as a function of time. From Figure I-26 it is seen that the core outlet temperature increases roughly 400°F in about 0.8 seconds. Since the curve shown on Figure I-26 is the mixed average coolant temperature for the entire core, a separate calculation has been done to determine the temperature response of the hot rod in the core. This calculation is discussed in Section 1.3.4.

1.3.4 Hot Rod Transient Temperature Calculations

The transient temperature distribution in the hot rod was calculated using an improved version of the TIGER II computer code. The model used for this calculation (hereafter referred to as the Full Length Fuel Rod Model) is shown on Figure I-27. As shown on Figure I-27, the fuel rod is divided into seven axial nodes. Each of the axial nodes contain five radial nodes in the fuel, three radial nodes in the cladding and a coolant node. For this calculation, the coolant temperature was assumed to be uniform around the circumference of the rod. The circumferential temperature variations associated with the coolant mass flow variation is treated separately and is discussed in Section 1.3.7. The power trace

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Figure I-25. DESIGN TRANSIENT

Figure I-26. CORE AVERAGE OUTLET TEMPERATURE
FOR DESIGN TRANSIENT

Figure I-27. FULL LENGTH FUEL ROD TRANSIENT HEAT TRANSFER MODEL

shown on Figure I-25 obtained from the FORE I calculation was used as input to the Full Length Fuel Rod Model. Results obtained from this calculation included the radial and axial temperature variation in the fuel meat and the fuel rod cladding and the axial coolant temperature distribution. As an example, Figure I-28 is a plot of the core coolant outlet temperature for the hot rod for the Design Transient. This figure can be compared to the mixed average shown on Figure I-26. This comparison shows that the peak coolant outlet temperature in the core is $\approx 40^\circ\text{F}$ higher than the mixed average temperature for the Design Transient. This difference in temperature is due partly to the transient heat transfer aspects of the problem, but more importantly, to the fact that a hot channel flow factor has been used in the analysis as discussed in the steady state calculations, Section 1.2.1. Figure I-29 is a plot of the axial temperature distribution in the cladding during the Design Transient obtained from the Full Length Fuel Rod Model. More detailed local cladding temperature calculations for the Design Transient are discussed in Section 1.3.7.

Calculations similar to the ones described above for the Design Transient have been repeated for the two maximum planned transients shown in Table I-7. Figure I-30 is a plot of the results for the maximum planned transient with the expected Doppler coefficient of $T \frac{dk}{dT} = -.0085$. Figure I-30 includes plots of the power trace and the average temperature rise similar to the results shown on Figure I-25 for the Design Transient. It will be noted that the maximum average temperature rise of 451°F for the planned transient shown on Figure I-30 is over a factor of three smaller than the 1553° temperature rise for the Design Transient shown on Figure I-25. This significant reduction in the size of the transient is reflected not only in the core outlet temperature but also in the maximum fuel temperature. The maximum fuel temperature reached for this planned transient is $\approx 3100^\circ\text{F}$ as compared to 5100°F for the Design Transient.

A summary of the results for planned transients assuming a Doppler coefficient of $T \frac{dk}{dT} = -.004$ which would be the minimum acceptable value for the Core II program is shown on Figures I-31 and I-32. Comparing

Figure I-28. COOLANT OUTLET TEMPERATURE FOR DESIGN TRANSIENT
(HOT ROD)

Figure I-29. AXIAL CLADDING TEMPERATURE DISTRIBUTION
 DURING DESIGN TRANSIENT (HOT ROD)

Figure I-30. MAXIMUM PLANNED TRANSIENT FOR $T \frac{dk}{dT} = -.0085$

Figure I-31. MAXIMUM PLANNED TRANSIENT FOR $T \frac{dk}{dT} = -.004$

Figure I-32. COOLANT OUTLET TEMPERATURE FOR MAXIMUM
 PLANNED TRANSIENT WITH $T \frac{dk}{dT} = -.004$ (HOT ROD)

the results on Figure I-31 to Figure I-30, it is seen that the maximum average temperature rise for the $T \frac{dk}{dT} = -.004$ case is approximately 100°F larger than the planned transient for the expected Doppler of $T \frac{dk}{dT} = -.0085$. This comparison indicates that the planned transient corresponding to that which could be run for the minimum Doppler coefficient of $T \frac{dk}{dT} = -.004$ is the most severe. Therefore, this planned transient has been further investigated for local temperatures and mechanical loadings and compared to the Design Transient in later sections (Section 1.3.7 and 1.5.3) of this supplement.

The hot rod power and flow factors used for the steady state calculations (See Table I-5) were also used for all the hot rod transient calculations. Coolant properties are taken from the Liquid Metals Handbook. Changes in coolant properties (such as heat capacity and viscosity) are small in sodium for the temperature range covered during the prompt critical transient. A value of $.304 \frac{\text{BTU}}{\text{lb-}^\circ\text{F}}$ was used. This is the heat capacity for an average temperature of 800°F, (700°F inlet and 900°F outlet). The heat capacity decreases slightly with increasing core outlet temperature but the decrease in viscosity associated with such temperature increases, increases flow slightly to compensate for this effect. (The minimum average heat capacity would be about $.302 \frac{\text{BTU}}{\text{lb-}^\circ\text{F}}$ corresponding to a 700°F core inlet and a 1200°F core outlet coolant temperature). The coolant properties are relatively easy to measure and have been measured numerous times so the error in the handbook values should be small.

1.3.5 Transient Temperature Distribution in the Segmenting Plug Region (Hot Rod)

The transient temperature distribution in the active fuel has been discussed in the previous subsection. Calculations have also been performed for the segmenting plug region similar to those presented for the steady state condition in Section 1.2.3. The same model used in the steady state calculations was used for the transient calculations. The power trace for the Design Transient shown on Figure I-25 which was calculated using the FORE I computer code was used as the energy input to the segmenting plug heat transfer model. Figure I-33 shows the steady state temperature distribution in the segmenting plug region at the start of

Figure I-33. STEADY-STATE ISOTHERMS (IN °F) AT START OF DESIGN TRANSIENT

the Design Transient (7 MW_t steady state power level). A significant point to note on Figure I-33 is that the segmenting plug temperatures are about 50 to 60° lower than the washer temperature. For this reason, only the transient temperature response of the washer is included in this supplement. Figure I-34 is a plot of the temperature response of the washer to the Design Transient. Shown on Figure I-34 is the maximum temperature of the washer and the volumetric average temperature of the washer. The significant point on Figure I-34 is that the washer temperature does not exceed about 1150° so that there is no concern about loss of strength of the structural material (washer, spring or segmenting plug) in the segmenting plug region. The maximum temperature in the U-shaped segmenting plug is lower than the washer (as was discussed above). Figure I-36 shows the volumetric average temperature response of the insulator pellet and the first fuel pellet adjacent to the insulator pellet during the design transient. Figure I-35 shows that despite the significant prompt heating in insulator pellets (15% conservatively assumed for these calculations; 11% predicted in Table III-11), the insulator pellets run very cool even for the Design Transient. The maximum volumetric average fuel temperature for the fuel pellet adjacent to the insulator pellet is shown on Figure I-35 as $\approx 4500^\circ\text{F}$. Since the peak volumetric fuel temperature reached in the fuel is in the vicinity of 4600°F , the segmenting plug region does not significantly depress the local fuel temperature.

The results discussed above indicate that the temperatures reached during the Design Transient are not high enough to be of concern from the standpoint of loss of structural strength of materials in the segmenting plug region. Detailed stress calculations have been performed for the region of the weld between the segmenting plug and the cladding. These calculations are discussed in Section 1.5.3.3 of this supplement. A series of calculations were also performed to determine the sensitivity of the results described above to the heat transfer coefficients assumed between the various surfaces in this region. Figure I-36 is a description of the model used for the calculations described above. Shown on Figure I-36 are the surface heat transfer coefficients (in $\text{BTU}/\text{Hr}\cdot\text{Ft}^2\cdot^\circ\text{F}$) used

Figure I-34. WASHER TEMPERATURE DISTRIBUTION DURING THE DESIGN TRANSIENT (HOT ROD)

Figure I-35. FUEL AND INSULATOR TEMPERATURE DISTRIBUTION DURING THE DESIGN TRANSIENT (HOT ROD)

Figure I-36. HEAT TRANSFER COEFFICIENTS FOR SEGMENTING
PLUG TRANSIENT HEAT TRANSFER CALCULATIONS
(HOT ROD)

in the analysis for the hot rod. The effect of varying these heat transfer coefficients by factors of 2 in various combinations have shown that these coefficients did not significantly affect the temperature distribution in this region. This is because the surface heat fluxes are low so that the temperature drop due to the resistance to the heat transfer at these surfaces is not significant relative to the temperature levels of interest here.

1.3.6 Temperature Distribution Throughout the Reactor Vessel During the Design Transient

The discussion in the previous sections has been limited to temperature variations of the coolant within the heated fuel region. The temperature at the core exit as shown in Figure I-26 increases approximately 400°F in 0.8 seconds. The affect of this temperature pulse at the reactor vessel nozzle exit and in the main components in the primary system (such as the IHX) are of concern and have been investigated. Figure I-37 is a summary of the results of this analysis. This figure shows temperature variations as a function of time at four locations in the reactor vessel. The curve labeled "Core Exit" is a repeat of the curve shown on Figure I-26. The curve labeled "Riser Exit" is the temperature variation at the top of the B_4C shielding rods (top of the fuel channels). The significant delay and attenuation in the core exit temperature pulse by the time it reaches the top of the fuel channels is due to transport delays and to the heat sink in the B_4C shield rods above the core. Further delay and attenuation results due to the storage in the short shielding channels in the downcomer section. This is shown by the curve labeled "Downcomer Inlet". The final curve on Figure I-37 is the temperature response at the reactor vessel nozzle exit. From this curve is seen that the temperature at the outlet nozzle increase at the rate of about 5°/sec with a maximum temperature change at the nozzle of less than 50°F. The temperature pulse shown for the vessel exit is further attenuated before it reaches the main IHX.

Figure I-37. COOLANT TEMPERATURE RESPONSE TO THE DESIGN TRANSIENT

1.3.7 Local Temperature Distributions During Design Transient

Mixed average coolant temperature distributions for the core and for the hot rod in the core have been discussed in the above sections. A large number of additional calculations have also been performed to investigate the local temperature variation around the fuel rods during these transients due to local coolant mass flow variations. The basic heat transfer model used for these calculations is identical to that described in Section 1.2.2 for the steady state local temperature distribution calculations. Results of these calculations for the nominal fuel rod position and for the two dimensional tolerance extreme positions previously discussed are summarized below. As discussed previously (Section 1.2.2) the heat transfer model does not include fuel nodes; instead, the assumption is made that the energy input to the cladding does not vary circumferentially. This is a conservative assumption (for both steady state and transient conditions) since any circumferential heat transfer within the fuel itself would tend to reduce the energy input to cladding at the positions where high temperatures occur. The effect of this additional circumferential heat transfer in the fuel would therefore be to reduce both the maximum cladding temperature calculated and the circumferential variations in the clad temperature from that calculated using this simplifying assumption. The energy input to the cladding for this local temperature calculation during the Design Transient was obtained from the results from the Full Length Fuel Rod Model discussed in Section 1.3.4 above using the following method. First, the energy input in the cladding nodes at the initial steady state power level was related to the fuel-to-clad surface heat flux. The instantaneous fuel-to-clad heat fluxes obtained in the Full Length Fuel Rod Model calculation for the Design Transient were then divided by the initial steady state heat flux to obtain multipliers for the heat generation input to the clad nodes. This time dependent power generation library was then input into the heat transfer model (Figure I-13) used to calculate clad and coolant temperature distribution locally in the hot rod. Figure I-38 is an example of the results obtained from this calculation.

Figure I-38. CIRCUMFERENTIAL COOLANT OUTLET TEMPERATURE DISTRIBUTION FOR THE DESIGN TRANSIENT (HOT ROD)

Figure I-38 shows the temperature distribution at the outlet of the hot rod at the point at which the maximum coolant outlet temperature is reached (at approximately .98 second after the start of the Design Transient). This temperature distribution is for the fuel rod at the nominal position. Figure I-39 is a plot of the variation of the outlet temperature at the circumferential position corresponding to 30 degrees (which is the minimum temperature) and at 150 degrees (which is the maximum temperature) during the Design Transient. Figure I-39 shows that the maximum temperature reached for the rod at the nominal position is slightly over 1200°F. At the same time, the minimum temperature is in the vicinity of 1030°F. A similar behavior is exhibited by the fuel rod cladding and has been used in the stress analyses which are discussed in Section 1.5.3 in this supplement.

These calculations have been repeated for the extreme dimensional tolerance positions discussed in Section 1.1.3. The results shown on Figures I-40 and I-41 are for the extreme dimensional tolerance position that would result if a straight fuel rod is moved over to within 55 mils of the tightener sleeve. For this extreme condition and for the Design Transient, the peak outlet coolant temperature approaches 1390°F. In addition, variations between the hot and cold nodes have increased from the nominal fuel rod position case. Similar results are shown on Figures I-42 and I-43 for the second extreme dimensional tolerance case in which the fuel rod moves over to within 65 mils of the adjacent fuel rod. Figure I-42 shows the temperature distribution around the hot fuel rod at the time at which the peak to local temperature occurs. Figure I-43 again shows the time response of the hot node and the cold node throughout the transient. As can be seen from Figure I-43, this second dimensional tolerance extreme case is the most severe. The peak temperature reached during this transient is 1500°F which is still 120°F below the normal boiling point of sodium or 320°F below the sodium saturation temperature if the system is being operated with the design 20 psig cover gas pressure. It should be emphasized that both of these dimensional tolerance extreme situations are not physically possible

Figure I-39. MAXIMUM AND MINIMUM COOLANT OUTLET TEMPERATURES DURING THE DESIGN TRANSIENT (HOT ROD)

Figure I-40. CIRCUMFERENTIAL COOLANT OUTLET TEMPERATURE DISTRIBUTION
FOR THE DESIGN TRANSIENT (HOT ROD)

Figure I-41. MAXIMUM AND MINIMUM COOLANT OUTLET TEMPERATURES DURING THE DESIGN TRANSIENT (HOT ROD)

Figure I-42. CIRCUMFERENTIAL COOLANT OUTLET TEMPERATURE DISTRIBUTION DURING THE DESIGN TRANSIENT (HOT ROD)

Figure I-43. MAXIMUM AND MINIMUM COOLANT OUTLET TEMPERATURES DURING THE DESIGN TRANSIENT (HOT ROD)

since the bowing gradient resulting from these calculated temperature distributions result in deflections of the rod which tend to correct this situation because the rod will not remain vertical at the tight spacings over its entire heated length. The significance of these calculations is that even for this extremely conservative calculation, the peak local temperature is still well below the boiling point of sodium for the Design Transient.

Similar calculations to those described above for the Design Transient have been repeated for the most severe maximum planned transient (for $T \frac{dk}{dT} = -0.004$). One set of these results are discussed in this section in order to make a comparison between the planned transients and the Design Transient discussed above. Figures I-44 and I-45 present results for the maximum planned transient comparable to the results shown on Figures I-42 and I-43 for the Design Transient.

1.4 CORE CHANNEL STRUCTURAL ANALYSIS

1.4.1 Introduction

The SEFOR core comprises a vertical bundle of thin-walled hexagonal tubes (fuel channels) arranged in a hexagonal array. Each fuel channel contains six fuel rods symmetrically arranged and a central tightener rod assembly. The fuel rods are located within the fuel channel by the central tightening assembly and spacer buttons on the channel wall (see Figure I-9). Therefore, thermal motions of the fuel rods are restrained in magnitude to that which can occur within the individual channel. Consequently, gross radial changes in core dimensions are determined by the response of the vertical bundle of fuel channels to the various operating conditions. Because of the reactivity effect associated with radial changes in core dimensions, the magnitude and reproducibility of such dimension changes were investigated to assure safe operation of the plant.

The channels, ideally, have no gaps between them and are held in a compact bundle by a circumferential row of radial springs which apply loads at four elevations. (See Figures I-46 and I-47.) At each elevation the spring acts on pads which fit the peripheral contour of the channel bundle and provide an approximately uniform line load circumferentially around the bundle of channels.

Figure I-44. CIRCUMFERENTIAL COOLANT OUTLET TEMPERATURE DISTRIBUTION DURING THE MAXIMUM PLANNED TRANSIENT (HOT ROD)

Figure I-45. MAXIMUM AND MINIMUM COOLANT OUTLET TEMPERATURES DURING THE MAXIMUM PLANNED TRANSIENT (HOT ROD)

$\frac{1}{6}$ Segment of Bundle

Figure I-46. TYPICAL CROSS SECTION OF CORE FUEL CHANNELS

Figure I-47. ELEVATION VIEW OF CORE FUEL CHANNELS

The springs are arranged in vertical pairs, each pair providing an equal radial load on the core at two elevations, which is introduced by a common torsion bar between the two load application points. Load is applied by a torque wrench and controlled in integral steps by a 48-tooth spline which locks the load pad to the torsion bar. The channels are supported vertically at their base by a gridplate. The mechanical details are such that the channel is free to take any radial position, hence, the radial location of the channels during operation is controlled entirely by the radial springs on the periphery of the bundle of channels.

The axial power distribution in the core is approximately a chopped-cosine. The temperature rise of the coolant and channel walls follows the integral of this power distribution as shown on Figure I-48. For simplification, a straight line approximation is used in the analysis in which the channel temperature increases linearly from the core inlet temperature (at the bottom of the core) to the core outlet temperature (at the top of the core). With this assumption and defining zero elevation as the bottom of the channel, the temperature distribution in the fuel channel is constant up to the bottom of the active fuel region ($\approx 6''$), increases linearly from $6''$ to the top of the active fuel region and is constant above the active fuel region. A gradient exists diametrically across each channel in the core region only, due to the core radial power distribution and the assumption of no coolant mixing within the fuel channel. These diametral gradients are assumed to increase linearly from the bottom of the active fuel region to a maximum value at the top of the active fuel region. The maximum value is assumed to exist from the top of the fuel to the tops of the channels. (Results from calculations indicate that this assumption is not unduly conservative.)

Table I-8 contains a summary of the diametral gradients used in the analysis of three specific reactor operating conditions. The first column in the table is for the normal steady state 20 MW_t operating condition. The nominal conditions are 700°F inlet, and a 120°F core temperature rise, giving a core outlet temperature of 820°F . The diametral gradients given in the column are for the five channels located on a typical radius going from the center of the core to the periphery of the core (see Figure I-46). The second column in Table I-8 lists the conditions

Figure I-48. CORE COOLANT TEMPERATURE AXIAL DISTRIBUTION

for the prompt critical transient analyzed. Both this transient and the transient corresponding to a severe accident condition (shown in Column 3 of the table) have been analyzed on a quasi-static basis. It will be noted that the core temperature rise (ΔT) used for the prompt critical analysis is slightly smaller than the maximum resulting for the Design Transient as discussed in Section 1.3.2 of this supplement, ($\Delta T = 360^\circ\text{F}$ in Table I-8 versus 440°F for the Design Transient). However, the conditions used are significantly more severe than the maximum planned transients described in Section 1.3.2 of this supplement so the slightly larger transient was not analyzed. In addition, the results of the calculations (described below) indicate that no problems would be encountered for the Design Transient.

Thermal hydraulic analyses of the core have shown that the diametral temperature gradients across the channels are (to good accuracy) directly proportional to the core ΔT . Since the core ΔT for the prompt critical transient analyzed is 3 times the 20 MW_t ΔT the diametral temperature gradients used for the prompt critical transient calculations have been taken to be 3 times the value used for the normal 20 MW steady state condition.

The accident condition shown in Table I-8 is a very severe condition which assumes loss of site power, no flywheel coast down, and failure of the reactor to scram. This accident is not assumed to be credible and has not been used as a design bases for determining the adequacy of the design. However, it does represent an upper bound on accident conditions where the core radial expansion is a significant reactivity effect, and in which structural temperatures get above the normal steady state or planned transient operating conditions. Under this extreme accident condition, it would be quite important that the core expand radially and maintain its radial geometry and not compact (due to channel bowing or to plastic deformation of the channel) as a result of the combination of higher stresses and higher material temperatures.

Table I-8
CONDITIONS USED IN CORE CHANNEL ANALYSIS

	Nominal S.S. Cond.	Prompt Critical Transient	Accident Cond.
Core Inlet Temp.	700	700	700
Core Outlet Temp.	820	1060	1450
Axial Coolant ΔT	120	360	720
*Diametral Channel ΔT			
Channel #1	4.5		
2	7.5		
3	13.0	Three Times	Six Times
4	20.0	Nominal	Nominal
5	29.5		

*Diametral gradient is zero at the bottom of the core and increases to the values shown at the top of the core. The axial variation of this gradient is approximately linear.

1.4.2 Channel Structural Analysis

1.4.2.1 General

As discussed in Section 1.4.1 above, the coolant temperature can be assumed to increase linearly through the active fuel region of the fuel channels. Each individual channel will, therefore, grow radially as a function of this temperature change through the active fuel length in the core. Figure I-49 shows the typical shape of a fuel channel due to the axial increase in channel wall temperature in the core region. The effect of this is to cause the channel bundles to grow radially and if all the channels are held in mutual contact, the bundle will have a conical shape through the core region and a straight cylindrical shape above and below the core region. Figure I-50 shows the free deflection pattern of the channels due to this radial growth in the core region. Also shown on Figure I-50 is the condition of all channels in the bundle in mutual contact along their entire length. This condition is referred to as a compact core geometry and serves as the reference for determining gapping of the channels for the various conditions considered.

Figure I-49. CONICAL GROWTH OF CHANNEL AT 20 MWt OPERATION CONDITIONS

Compact Core Geometry

Unrestrained Geometry Due to Conical
Channel Growth **Figure I-50.** CHANNEL BUNDLE SHAPE FOR 20 MWt OPERATING CONDITIONS

For this condition, the top of the core becomes a pivot point about which the channels (except for the central channel) must be bent in a radial plane. The natural deflection shape when this is done by spring loads at the design locations shown on Figure I-47 is concave inwards in the core region, and gapping due to this is not reduced by increasing the magnitude of spring forces at the spring locations. Fortunately, the diametral gradient across the fuel channels due to the radial power gradient in the core region tends to counteract this. Figure I-51 shows the response of the fuel channels to this diametral gradient alone when no restraining force is applied. As can be seen on Figure I-51, the diametral gradient through the channels in the core region causes the bowing in the channels to be concave outward. Consequently, the gapping between the channels which would result from each of these gradients, applied independently, tends to be compensating when the bundle is compacted at the spring elevation by the spring loads.

1.4.2.2 Beam Analysis of Channels

The beam analysis for the fuel channels were performed with the aid of the digital computer program. The radial gradient is specified along the beam, and boundary conditions on displacement or load are prescribed at discrete points along with length of the beam. The program was set up to compute deflections and stresses along the beam and the reaction loads at the prescribed restraint locations. For each set of calculations, the displacement pattern for the individual channels is plotted on the "compacted core" geometry (defined on Figure I-50) from which points of interference can be seen and removed by further iteration. This comparison also gives the radial deviation (gapping) of the channels from the compact core configuration. Due to the conical growth of the channels in the core region, the top of the core becomes a pivot point about which the channels are bent by the inward radial spring forces. The outward bow of the channels (resulting from the radial gradient across the channels) is also resisted by bending the channel about this pivot point with the spring forces from the torsion bar system. For cases where the upper spring loads applied do not completely remove the bow, the pivot point is somewhat lower than the top of the core. Being indeterminate, this location is found by trial and error.

Figure I-51. CHANNEL BUNDLE SHAPE DUE TO CHANNEL DIAMETRAL TEMPERATURE GRADIENTS (20 MWt)

Channels on the periphery of the core are the most highly loaded since they have the largest diametral ΔT causing bowing and because the amount of bending required to make the channels conform to the compact conical core geometry is largest at the periphery. Since four sets of springs are used (see Figure I-47), these loads in conjunction with internal reaction between channels make the beam model of each channel statically indeterminate. The internal reactions can be obtained in two ways. These are (1) by bearing on the next inward channel and (2) by bridging circumferentially as an arc. The first condition would exist if the channel cross section were completely rigid so that no deflection occurs under the concentrated loads. The second effect occurs if the cross sections of the channels distort enough to bridge the gap created when the channel is outboard of the fully compacted position. This distortion occurs adjacent to concentrated load points.

The reactions due to arching cannot be included directly in the computation so the point at which compatible beam deflection and channel cross section distortion is reached is found by trial and error. The ring in the channel below the lower spring pads (at the bottom of the core) is assumed to make the channel essentially rigid at this elevation so distortion and arching does not occur. Therefore, the deflection is set as the boundary condition at the bottom of the channel, and at the top of the core, and loads applied at the three remaining spring elevations (57-1/2, 70 and 95" from the bottom of the channel). Several calculations are then made until the radial deflection of the channel from the compact geometry is compatible with the local distortion that occurs under the load.

Results of calculations are summarized in the following paragraphs for channel #5 which has the largest load, and the highest bending stresses of any of the channel positions in the core. Results are presented for both the condition of the rigid channel analysis and for the arching condition. These two conditions are believed to bracket the actual situation in the core. This conclusion is arrived at because the fuel channels are not rigid and tend to deflect under the concentrated load, and because the flexibility analysis used to calculate the local distortion (which causes the arching) is conservative

so that it over-predicts the flexibility of the channel under the applied load.

Some typical results of these calculations are shown on Figures I-52 through I-54. Figure I-52 is a plot of the deflection shape of the centerline of the #5 channel and the bending stresses that occur in this channel for the prompt critical transient condition. As noted on Figure I-52, the results shown are for the assumption that the channel is rigid and does not deform under the local loads that are applied. It will be noted from Figure I-52 that the maximum gapping between the compact core configuration and the centerline of the #5 channel is only 9 mils. This amount is distributed between the five channels going out radially so that the actual gap between the #4 and #5 channels would be less than half this value. Figure I-52 also shows that the maximum bending stress is 7500 psi and occurs at the top of the core. The results shown on Figure I-52 are for the assumption that 150 lbs. of load is applied to the #5 channel at elevations 57-1/2" and 70". Because of the torsion bar arrangement, the same load would also be applied at elevation zero and at elevation 95". The difference between the loads shown at elevation zero and elevation 95" and 150 lbs. does not cause further deflection of the channel but is taken as a compression load. Figure I-53 is identical to Figure I-52 except that the load being applied to channel #5 has been doubled to 300 lbs. to see the affect of an increase in load from the torsion bar system on the deflection and stresses in the channel. The significant thing shown on this figure is that the gapping in the core is increased only 1/2 mil from 9 to 9-1/2 mils, and the bending stress is increased only slightly for a factor of two increase in the applied load. Figure I-54 is a similar plot of the deflection mode and stresses in the #5 channel for the prompt transient condition, but with the assumption that arching occurs so that the channel above the top of the active core is not pushed into contact with the compact core geometry. The significant points to note on this figure are that (1) the gapping between the channel centerline and the compact core configurations have been slightly reduced from 9 to 8 mils and (2) the bending stress has also

Figure I-52. CHANNEL DEFLECTION FOR THE PROMPT POWER TRANSIENT CONDITION (LOAD CONDITION #1)

Figure I-53. CHANNEL DEFLECTION FOR THE PROMPT POWER TRANSIENT CONDITION
(LOAD CONDITION #2)

Figure I-54. CHANNEL DEFLECTION FOR THE PROMPT POWER TRANSIENT CONDITION (FLEXIBLE ANALYSIS)

been reduced 20-25%. From this it can be concluded that the analyses for the rigid channel condition gives the largest gapping and the highest bending stresses for the two bounding conditions considered.

Calculations similar to those described above for the prompt critical transient have been repeated for the 20 MW_t and for the accident conditions which were described in Table I-8. A summary of the results from these calculations is given in Tables I-9 and I-10. Only the results from the periphery channel (channel #5) are included because the stress level is the highest in this channel. The gapping shown in the table is the maximum displacement between the compact core configuration and the centerline of channel #5 that occurs in the active core region. Table I-10 is a comparison of the code allowable values (for the temperatures that exist for the three conditions analyzed) and the calculated stress. It is seen that for the steady state and prompt transient condition the maximum stresses are well below code allowable and no permanent deformation would occur. The stresses given in Table I-10 are for bending only. Tensile stresses due to the operating pressure gradient across the channel wall is approximately 1000 psi and does not change the above conclusion.

For the accident case, the maximum stress, assuming the channels are rigid, is slightly above the code allowable, while the stress, assuming arching action to occur, is below code allowable. From this it is concluded that while it is possible that a small amount of permanent deformation would occur in the accident condition, this deformation would be slight and would not result in a gross change in the core dimensions that could cause any significant reactivity change.

Table I-9

DEFLECTION OF CHANNEL AT CORE PERIPHERY (CHANNEL #5)

<u>Operating Condition</u>	Maximum Gapping	
	<u>Rigid Channel Analysis</u>	<u>Flexible Channel Analysis</u>
20 MW _t Steady State	3.5 mils	3 mils
Prompt Critical Transient (3 x 20 MW _t values)	9 mils	8 mils
Accident Condition (6 x 20 MW _t values)	16 mils	15 mils

Table I-10

BENDING STRESSES IN CHANNEL AT CORE PERIPHERY (CHANNEL #5)

<u>Operating Cond.</u>	Max. Bending Stress		<u>Max. Chan. Temp.</u>	<u>ASME Code Yield Strength (304 St. Steel)</u>
	<u>Rigid Channel Analysis</u>	<u>Flexible Channel Analysis</u>		
20 MW _t Steady State	3000 psi	1800 psi	850°F	16,000
Prompt Critical Transient (3 x 20 MW _t values)	7500 psi	5500 psi	1100°F	14,300
Accident Condition (6 x 20 MW _t values)	12000 psi	9000 psi	1450°F	10,000

1.4.2.3 Design Load for the Torsion Bar Clamping System

The total radial load required from a row of springs (torsion bars) at a given elevation is the sum of the load required by each channel of the bundle at that elevation; the load in an individual spring being the total load divided by the number of springs. From the results of the structural analysis of the channels, it was concluded that 250 in-lb of torque for the main torsion bar spring (torsion bar in the active core region) is adequate to limit the gapping in the core for the transient condition to magnitudes indicated by the results shown on Figures I-52 through I-54 (the requirement for the steady state operating condition is less than this). The torsion bar system used in the design is capable of applying

loading in the increments of 250 in-lbs. These increments result because a spline arrangement is used in loading the torsion bar system. For this reason, the applied load can vary by approximately 250 in-lbs. Since it was shown (see Figure I-53) that increasing the load has a negligible effect on the gapping or the stress level in the channels, the 250 in-lbs torque has been increased by a factor of two for margin to cover possible long term creep relaxation of the torsion bars. With this factor of two, and the 250 in-lbs increments for loading, the design torsion bar load is 500 to 750 in-lbs. If the channels behave rigidly, this torsion bar loading corresponds to a load of 190-285 lbs on channel #5 at the bottom of the core and at the 57-1/2" elevation. With the channel cross section flexibility expected, the beam load will be less than 190 to 285 lbs and the excess will be carried by the arch-action discussed before.

The stress level in the lower torsion bar (which is 9/16" in diameter and approximately 50" long), is 21,000 psi for the maximum 750 in-lb loading. This maximum stress level gives a large margin for degradation of the stress rupture strength of the Inconel 718 used for the torsion bars. The stress rupture strength of Inconel 718 at 1000°F for 100,000 hours (>10 years) is 60,000 psi.

The loads for the upper torsion bars are considerably lower than for the lower set. Using procedures similar to the one described above for the lower torsion bars, the required pre-load for the upper torsion bars is 125 in-lb to 180 in-lb. The maximum shear stress in the upper bar at 180 in-lb is calculated to be 30,000 psi. This maximum stress is also well below the 60,000 psi stress rupture strength (100,000 hours at 1000°F) for Inconel 718.

During steady state operation, the torsion bars and the torsion bar support system operates essentially at the temperature of the fuel channels. For rapid transients (or the severe accident condition considered) the core will expand relative to the torsion bar system and increase the load being applied by the springs. This effect has been analyzed and it is found that for the worst accident condition, the

load being applied by the torsion bar system would increase only about 5%. This slight increase in load would have a very small affect on the gapping and bending stress in the fuel channels.

1.4.2.4 Local Deformation of the Channels

Calculations have also been performed to investigate the local stresses and deformations under the localized loading that occurs at the application of the spring loads or at the reaction point at the top of the core. The results from these calculations have shown that the stress levels and deflections are small at these points and should not result in permanent deformation. In addition, a full scale one-sixth section mock-up of the SEFOR fuel channels was tested as part of the Pre-Operational R&D Program⁽⁸⁾. The mock-up included the torsion bar system and had provisions for heating to investigate the affect of thermal expansion on the bundle of channels. This mock-up demonstrated that there was no tendency of bridging in the outer channel which could result in interior channels being loose. This was demonstrated by clamping up the core and measuring the loads required to move channels vertically at various locations in the one-sixth section. Tests were also run to determine the affect if any, on the force required to remove individual fuel rods with the channels clamped and unclamped. These tests demonstrated that there was no significant difference between the load required to remove rods in the clamped and unclamped condition. This result verified the calculation which showed that the local deformations under the contact points would be small.

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1.5 FUEL ROD STRUCTURAL ANALYSIS

1.5.1 Introduction

The strength theory, terminology and stress categories presented in the ASME Boiler and Pressure Vessel Code, 1965, and its interpretation are used as a guide in the mechanical design and stress analysis of the SEFOR fuel rods. The calculations cover both the steady state (and quasi-steady state) and transient operating conditions.

The steady-state analyses include both the static power operation and the oscillator tests. The latter are described in Section 12 of the FD&SAR. The oscillator tests can be included as a quasi-static condition since analog studies show that the fuel cladding temperature response will be significant only for the low frequency oscillator tests and for these conditions quasi-static conditions exist. The design criterion for the steady-state analysis is that the fuel rod have infinite life (from a thermal fatigue standpoint). Since the stresses in the fuel rod are predominantly thermal in origin, this criterion can be met by requiring that the combined stress be less than twice the at-temperature yield strength. The combined stress values are obtained by combining the principal stresses at a point using the maximum shear theory of failure. This failure theory is the most conservative of the common failure theories and is recommended in Section III of the ASME Code.

The transient tests planned as part of the SEFOR test program include both sub-prompt and prompt critical transients as described in Reference 6. These transients result in radial temperature gradients across the fuel cladding wall that are significantly larger than the steady-state values. Consequently, the combined stresses in the fuel cladding are larger than the $2\sigma_y$ limit and the life of the cladding may be limited by thermal fatigue.

The thermal calculations for both steady-state and transient conditions have been discussed in Section 1.2 and 1.3 of this supplement. The results from these thermal calculations are used directly in the stress calculations described in the following subsections.

1.5.2 Structural Evaluations for Core Components at Steady State Conditions

1.5.2.1 Structural Evaluation of the Fuel Rod

The structural analysis of the fuel rod under steady state conditions has included investigations of the stresses in the fuel cladding opposite the fuel, in the segmenting plug region and the end closures of the fuel rod. The results of these calculations have shown that the critical location is in the cladding opposite the fuel.

The structural loading of the clad is predominantly thermal in nature. The source of these thermal loads are: (1) the radial heat flow through the cladding, (2) the local stress due to the non-linear circumferential temperature variation in the cladding and (3) the bending stresses due to bowing gradient resulting from the linear portion of the circumferential temperature variation in the cladding. The only other source of loading that must be considered for the fuel clad is pressure loading due to the internal pressure of the gas inside the fuel rod. The stress due to this pressure loading is small relative to the yield strength of the cladding. This is the reason that the design criterion for the fuel cladding can be taken to be that the maximum combined stress is less than twice the at-temperature yield strength of the material. Using this criterion, local plastic deformation of the cladding wall (on the ID and OD of the tube) will occur on the first cycle to the steady state operating condition, but no further plastic strain will occur on succeeding cycles.

The thermal stress resulting from the radial heat flow through the cladding and the pressure stress due to the internal pressure in the fuel rod are calculated using standard equations. The stresses resulting from the circumferential variation in the cladding temperature are not as straightforward and are discussed in this section. The temperature distributions in the cladding near the top of the active fuel region where the peak cladding temperature and maximum local temperature gradients occur are shown on Figure I-17 (for the average fuel rod in the core), and on Figures I-21 and I-22 for the nominal and the two extreme tolerance positions within the fuel channel for the hot rod in the core. As discussed in Section 1.2, these temperature calculations include peaking factors on both the power and the flow for the hot rod calculation.

The method for calculating the local stress due to the non-linear portion of the circumferential temperature variation can be explained by referring to Figure I-17. This figure shows the circumferential temperature variation in the average rod near the top of the core and the linear component of this distribution. This linear gradient is referred to as the bowing gradient in this analysis. If it is assumed for the moment that the rod is unrestrained and free to grow as determined by the bowing gradient in the rod, deformation (not stress) will result due to the linear portion of the circumferential distribution. However, as can be seen, the local temperature of the cladding varies significantly from the local temperature corresponding to the bowing gradient and local constraint will result. For example, referring to Figure I-17, at the 30° circumferential position, the local temperature is approximately 12°F lower than the temperature of the linear temperature distribution at this point. The clad material in this region, therefore, will be in tension since this material will not have grown as much as the material at adjacent positions. Similarly, at the 60° position, the local temperature is approximately 25°F hotter than the temperature of the linear temperature distribution so that this material will be in compression. These tensile and compressive axial stresses are referred to as the local stresses in this discussion. Figure I-55 shows the circumferential variation of the local stress for the average fuel rod at the nominal position. Note that in this case the maximum local stress is ≈ 7500 psi compressive. Note that this stress is a longitudinal stress. The non-linear clad temperature distribution causes only a small tangential stress.

The deflection of the fuel rod under the influence of the bowing gradient was discussed briefly in Section 1.2 because of the influence of this deflection on the thermal-hydraulics. The deflection mode of the fuel rods was calculated in a manner similar to that used for the fuel channels as discussed in Section 1.4 using a digital computer program to solve the beam equations. The rigid support points which act as boundary conditions for these calculations have already been discussed in Section 1.2. These points include spacer

Figure I-55. LOCAL STRESS VARIATIONS IN AVERAGE ROD (20 MWt)

The method for calculating the local stress due to the non-linear portion of the circumferential temperature variation can be explained by referring to Figure I-17. This figure shows the circumferential temperature variation in the average rod near the top of the core and the linear component of this distribution. This linear gradient is referred to as the bowing gradient in this analysis. If it is assumed for the moment that the rod is unrestrained and free to grow as determined by the bowing gradient in the rod, deformation (not stress) will result due to the linear portion of the circumferential distribution. However, as can be seen, the local temperature of the cladding varies significantly from the local temperature corresponding to the bowing gradient and local constraint will result. For example, referring to Figure I-17, at the 30° circumferential position, the local temperature is approximately 12°F lower than the temperature of the linear temperature distribution at this point. The clad material in this region, therefore, will be in tension since this material will not have grown as much as the material at adjacent positions. Similarly, at the 60° position, the local temperature is approximately 25°F hotter than the temperature of the linear temperature distribution so that this material will be in compression. These tensile and compressive axial stresses are referred to as the local stresses in this discussion. Figure I-55 shows the circumferential variation of the local stress for the average fuel rod at the nominal position. Note that in this case the maximum local stress is ≈7500 psi compressive. Note that this stress is a longitudinal stress. The non-linear clad temperature distribution causes only a small tangential stress.

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Figure I-55. LOCAL STRESS VARIATIONS IN AVERAGE ROD (20 MWt)

buttons, dimples on the fins attached to the tightener sleeve, and dimples in the tightener sleeve itself. The most significant results obtained from this analysis are the deflection mode of the fuel rod and the bending stresses that result due to the constraints offered by the support points in the fuel channel. A rather extensive series of these calculations were performed to provide guidance for the determination of the axial location of the various spacer points and then to demonstrate the adequacy of the chosen spacer configuration to maintain adequate coolant flow around the rod. The results from these calculations show that under the worst dimensional tolerance extreme conditions, the fuel rod will deflect until it comes in contact with the rigid supports within the channel. As discussed in Section 1.2, the thermal-hydraulic analysis for the fuel rod has considered the case where the rod is moved to these extreme positions. The bowing calculations have been performed with the conservatism introduced by the excessive bowing gradient resulting from computing that the rod is at this position along its entire length. This cannot physically happen as shown for a typical deflection mode shape on Figure I-20, i.e., the rod may be at the minimum spacing at one axial location but the resulting bowing gradient causes it to bow away from this location at other axial positions.

A summary of the steady state stress analysis is tabulated in Table I-11. Table I-11 shows the longitudinal stresses for the hot rod and the average rod at their nominal positions and also for the hot rod at the worst dimensional tolerance extreme position. As mentioned in Section 1.5.1, stresses were combined using the maximum shear theory of failure. This theory is defined by the ASME Code for Nuclear Vessels (Section III) in terms of principal stresses. Yielding is considered to occur if the greatest algebraic difference of principal stresses at a point is equal or greater than the yield stress for the material.

Radial stresses in the clad are small and may be taken as zero. For all cases considered, the dominant longitudinal (σ_z) and circumferential stresses (σ_θ) are thermal in nature and are of the same magnitude. In addition, the longitudinal stress (σ_z) is always larger than the tangential stress (σ_θ) because the local stress

contribution to the longitudinal stress at critical locations is always greater than the difference (factor of two) between the tangential and longitudinal stresses due to the internal pressure load. Thus the maximum combined stress is the difference between the longitudinal stress (σ_z) and the radial stress (σ_r) which is simply σ_z (since $\sigma_r \approx 0$). This is the reason that only longitudinal stresses are tabulated in Table I-11. Comparison of the individual longitudinal stresses shown in Table I-11 verifies the previous statement regarding the relatively small value of σ_z due to the internal pressure. The local stress and bending stress for the tolerance extreme position in the hot rod use the temperature distribution shown in Figure I-22. This tolerance extreme position results in higher cladding temperatures and larger temperature gradients than the other tolerance extreme position shown on Figure I-21. Table I-12 is a comparison of the maximum combined stress (using the individual stresses given in Table I-11) to twice the at-temperature yield strength again for the three conditions summarized in Table I-11. It will be noted that for the nominal position, both the average rod and hot rod combined stresses are significantly below the criterion of less than twice the at-temperature yield strength. In the tolerance extreme position, the maximum combined stress approaches the $2\sigma_y$ criterion. However, this situation is considered acceptable because of the conservatism that exists in the thermal calculations for this condition. Even if local stresses in excess of $2\sigma_y$ should occur, failure would not be expected since the fatigue life for this condition would still be very large (of the order of 10^6 cycles to failure).

The results presented above are for 20 MW_t operating condition with the nominal 700°F inlet and 120°F core temperature rise. As discussed in Reference 6, experiments are planned in which the core inlet temperature is changed from 700°F value and in which the core coolant flow rate does not match the reactor power. These steady state conditions result in outlet temperatures greater than 820°F and core ΔT 's greater than 120°F. Figure 4-1 of Reference 6 is a summary of the operating map that was used to evaluate the design

of the fuel. The stress calculations have been repeated for various positions along the boundary of the operating map. The results from these analyses demonstrated that the maximum combined stress levels in the fuel rods at any point within the operating map are always less than twice the at-temperature yield strength for the 316 stainless steel cladding, which is the design criterion for the fuel rod.

1.5.2.2 Choice of Fuel Clad Thickness

The choice of the .040" clad thickness is based on a trade-off between thermal stresses and stresses due to internal mechanical or gas loadings. Referring to Table I-11 and I-12 it is seen that any increase in clad thickness would result in an increase in the combined stress, i.e., the thermal stress (due to radial heat flow) would increase considerably more than the pressure stress would decrease. This occurs because both stresses are linear functions of wall thickness and the thermal stress is much larger than the pressure stress). In addition, the increase in clad wall thickness would necessitate a decrease in the coolant flow channel dimensions which would worsen the circumferential mass flow distribution problem and the associated stresses. The alternate ways of increasing cladding thickness without reducing coolant flow channel dimensions such as (1) increasing the basic fuel channel dimensions, or (2) decreasing the fuel pellet OD are not desirable because of neutronics and safety reasons. A study of the additional restraint to fuel rod distortion in the event of fuel-clad interaction (either during steady state or transient operation) indicated that the additional restraint offered by the thicker clad (for the increased thickness feasible from a thermal stress viewpoint) was not significant enough to warrant the use of greater than .040" thick cladding. It should be noted that the fuel is designed specifically to avoid the fuel-clad interaction and this loading is not included in the structural evaluation. Tests performed under Task 3 of the Pre-Operational Research and Development Program (Reference 9) have demonstrated that no appreciable fuel-clad interaction will occur for the fuel design used. **Tests** under Task 3 also investigated the affect of clad thickness on fuel performance. The results of this

Table I-11

LONGITUDINAL STRESSES IN THE FUEL ROD CLADDING (20 MW_t)

Location	Pressure Stress (psi)	Thermal Stresses (psi)		
		Radial Heat Flow	Local Stress	Bending Stress
Average Rod (Nominal Position)	2560	-9000	-7400	-3580
Hot Rod (Nominal Position)	3150	-13,500	-15,100	-6280
Dimensional Tolerance Extreme	3150	-13,000	-19,250	-9800

- Note: 1. Plus numbers denote tensile stresses, minus numbers compressive.
2. The longitudinal stresses given are at a point about 7 inches from the top of the active fuel which is the point where $\int \sigma_z$ is a maximum.

Table I-12

COMPARISON OF MAXIMUM COMBINED STRESS TO $2\sigma_y$

Location	Combined Stress (psi)	Maximum Local Temperature (°F)	$2\sigma_y$ (psi) (At Temperature)
Average Rod (Nominal Position)	17,400	890	44,100
Hot Rod			
Nominal Position	31,800	960	42,800
Dimensional Tolerance Extreme	38,900	1065	40,800

investigation are reported in Reference 9. These tests demonstrated that there is no significant change in fuel behavior for the range of cladding wall thicknesses that could be considered for the SEFOR application.

1.5.2.3 Gas Pressure Considerations

The internal pressure loading of the fuel rod is discussed in the previous section (1.5.2.3). The gas pressures are based on 100% release of fission gases at the peak design burn-up of 1500 MWD/Ton. The calculations also include the condensible and non-condensable gases in the fuel using the specification limits of 30 ppm and 50 microliters per gram, respectively.

Gas buildup in the special rods (BeO , B_4C) in the core and in the extension rods have also been considered. The stresses resulting from the gas buildup at end of life conditions are negligible.

1.5.3 Structural Evaluation of Core Components During Transient Operation

1.5.3.1 General

The thermal and thermal-hydraulic analysis for the fuel rod for the Design Transient and the largest planned prompt critical power transient have been discussed in Section 1.3. The temperature distributions obtained from these calculations were used to calculate the stresses in the fuel cladding, the segmenting plug and the top end plug. Thermal transients that occur during refueling when a fuel rod (at room temperature) is lowered into the sodium in the reactor vessel (350-400°F sodium) has also been considered.

The stresses examined for the Design Transient are primarily due to the thermal loading introduced by the radial and circumferential temperature gradients in the cladding and by the thermal (temperature) mismatches at the fuel rod segmenting plug and top end closure. The calculations have been limited to elastic analysis procedures. The final step in the calculation is the determination of the fatigue life of the fuel using the elastically calculated stresses and the thermal fatigue curves presented in the interpretation of Section III of the ASME Boiler and Pressure Vessel Code, Case 1331-1.

1.5.3.2 Transient Thermal Analysis of the Fuel Rod

The transient thermal analysis for the fuel rod cladding and for the segmenting plug has been described in Section 1.3. The heat transfer models and the conditions analyzed and discussed in Section 1.3 supply the temperature and temperature distribution information required for the structural analysis of the cladding and for the segmenting plug during the transient testing. A similar detailed heat transfer model was developed for analyzing the temperature gradients that occur in the top end plug due to the coolant pulse which the top end plug is subjected to as a result of the prompt critical transient tests. Figure I-56 is a sketch of the nodal arrangement used for the heat transfer calculation. It should be noted that with the modified butt weld used for welding the top end plug to the tube (see Section II of this supplement for description), changes in cross section are gradual in the weld region so that stresses due to thermal mismatches are small.

In addition to the prompt critical transients, the fuel rod is also subjected to thermal transients during normal refueling. During this operation, the fuel (which may be at room temperature) is lowered into the pool of sodium inside the reactor vessel which will be in the temperature range of 350 to 400°F. Temperature gradients in the fuel rod for this refueling operation have been investigated and evaluated to determine structural loadings. The main concern in this case is the thermal mismatch which will occur at positions in the fuel rod where cross sections vary significantly. These points include the bottom end plug, segmenting plug, and the top end plug of the fuel rod. A heat transfer model similar to the one described in Figure I-56 for the top end plug was developed for the bottom end plug. The heat transfer models for these various regions were used to determine temperatures and temperature gradients that would occur for the condition of lowering the fuel rod into the reactor at the maximum rate possible with the fuel handling equipment (60 in/min) and the resulting loadings evaluated.

1.5.3.3 Structural Evaluation of the Segmenting Plug and End Plugs

The loadings on the end plugs and the segmenting plug in the SEFOR fuel rod for the transient conditions are predominantly thermal. These

Figure I-56. HEAT TRANSFER MODEL FOR FUEL ROD TOP END PLUG REGION

thermal loads are the result of temperature mismatches that occur in adjacent shells of differing cross section. These transients occur both during the prompt-critical transients and during normal refueling. The transient temperature distributions in these various shells have been calculated as discussed previously in Section 1.5.3.2. The thermal mismatches along with pressure loading due to the internal pressure in the fuel rod were used as input to a digital computer code which solves for the deflection, loads and stresses in the shells. This computer program is a general purpose code capable of analyzing a structure of revolution made up of shells, rings, flange joints and other elastic systems under the influence of loadings that are symmetric about the axis of revolution. Computer codes of this general type and capability are in wide use in the fields where detailed analysis of shells are required. Table I-13 is a summary of the maximum combined stress (using the maximum shear theory of failure) for the end plugs and the segmenting plug for the transient conditions considered. Since these stresses are in excess of twice the yield strength of material, the fatigue life of the fuel rod at these locations for the two transients (the Design Transient and the refueling case) has been evaluated. The results of this evaluation are given in Section 1.5.3.6 and compared to the fatigue life predicted for the cladding. As discussed in Section 1.5.3.6, the critical location in the fuel rod is in the cladding opposite the fuel, i.e., the predicted fatigue life in the end plugs and the segmenting plugs is significantly larger than the predicted fatigue life of the cladding.

The welds that join the end plugs to the tube have been located and the weld designs selected so that the thermal stresses in the region of and adjacent to the welds are low. The stresses given in Table I-13 are at positions in machined parts of the end plug. This design approach reduces the probability of a failure in the end plug region.

This design approach was not feasible for the segmenting plug weld design. The segmenting plug weld is located in the region of maximum bending stress for transient operation. This bending stress is due to the thermal mismatch between the tube and the segmenting

Table I-13

MAXIMUM COMBINED STRESS IN THE END PLUGS AND SEGMENTING PLUG

<u>Location</u>	<u>Transient</u>	<u>Maximum Combined Stress</u>
Bottom End Plug	Refueling	45,800 psi
	Design Transient	Negligible
Segmenting Plug ⁽¹⁾	Refueling	52,800 psi
	Design Transient	104,000 psi
Top End Plug ⁽²⁾	Refueling	54,800 psi
	Design Transient	46,000 psi

(1) These stresses occur at the cladding OD adjacent to the electron beam weld which joins the cladding and the segmenting plug.

(2) These stresses occur in the machined part of the top end plug at the elevation corresponding to the end of the extension rod sleeve (see Figure I-56).

plug that occurs during the thermal transient. The segmenting plug was designed with a U-shaped cross section (see Section II for description) in order to minimize the thermal discontinuity stress and increase the predicted fatigue life of this weld joint. (The flexibility of the "legs" of the segmenting plug is approximately the same as the tube wall). A fatigue failure in this weld would probably be in the direction parallel to the axis of the tube which would not affect the integrity of the fuel rod. The possibility of the fatigue failure propagating around the entire circumference of the weld and allowing the bulkhead to move relative to the tube has been considered. This type of failure is considered unlikely and if it should occur, the resulting reactivity effect from the motion of the fuel within the rod (the upper fuel column falling down on top of the lower fuel column) would be negligible.

1.5.3.4 Thermal Shock Tests of SEFOR End Plugs and Segmenting Plug

A series of thermal shock tests were performed on the fuel rod as part of Task 5 of the Pre-Operational R&D Program on SEFOR in which the thermal shock was imposed on a UO_2 filled rod by flowing a slug of hot sodium past the rod.⁽¹⁰⁾ The temperature pulse in the coolant ranged from the 700° initial value to a peak temperature of 1300°F in approximately 1 sec. This coolant temperature pulse is approximately 25% larger than that resulting from the Design Transient discussed in Section 1.3. The rods tested had several different weld designs and a segmenting plug design which had calculated stress levels that are in excess of those calculated for the SEFOR fuel rod. These rods were subjected to approximately 50 of these large temperature transients with no sign of failure of the material. While 50 cycles is below the predicted fatigue life for any of the weld designs, this test does give confidence that the fuel rod design can withstand the limited number of prompt critical transients that are planned in the SEFOR test program.

1.5.3.5 Structural Evaluation of the Fuel Rod Cladding

The loading of the fuel cladding opposite the fuel has been investigated for the prompt critical transient test condition and for the refueling condition. The loadings for the refueling condition are quite small and present no problem, For the prompt critical

transient condition, the thermal loading is the most critical. The thermal stresses are principally due to radial and circumferential gradients in the cladding. The pressure stresses due to the internal pressure from fission gases in the gas plenum have also been included but were found to be relatively small.

Similar stresses at a point have been combined to produce the maximum principal stresses. Principal stresses have been combined according to the maximum shear stress theory of failure to compute the maximum stress comparable to uniaxial tensile data. The analyses have been limited to elastic procedures. For thermal stresses, the yielding generally is local and the strain components maintain a constant relation during yielding so that an elastic analysis with the stress reduced by the appropriate stress-strain relation may be employed without gross error resulting.

The experimental data available for evaluating thermal fatigue is based upon values obtained from constant temperature mechanical stress reversal tests. In most of these tests, the temperature is maintained constant and the stress is mechanically reversed to produce fatigue. In the case of SEFOR, the stress is due to temperature changes in the clad during the prompt transient testing. The stress is produced by alternate heating and cooling and the tensile and compressive stresses occur at different temperatures. The affect of the mechanically produced plastic strain vs. the thermal produced plastic strain on the fatigue data is not known. For this evaluation it is assumed that the fictitious stress (strain) produced by the thermal loading is equivalent to that produced by the mechanical loading. The values utilized for assessing the low cycle fatigue life of the cladding are taken from fatigue curves in Section III of the ASME Boiler and Pressure Vessel Code and Code Case 1331-1. These curves contain a factor of approximately ten conservatism in the number of cycles to failure for a given alternating stress range.

The combination of stresses for the cladding opposite the fuel illustrates a number of problems inherent in the choice of the proper combination of stresses and the criterion for evaluating these stresses. The principal stress contribution is from the large radial temperature

gradient in the tube wall opposite the fuel. This gradient results when the fuel temperature rises rapidly during the prompt critical transient. This condition produces large compressive stresses at the interior of the tubes due to the rapid rise in temperature of the material at the tube ID. Pressure stresses are relieving at this location and are not included. The other stress contribution comes from the non-linear portion of the circumferential temperature variation in the cladding discussed in Section 1.3. In addition, the restraint of the bowing gradient by the supports in the core contribute to bending stress. The largest combined stress occurs at the inner surface of the wall of the tube and is compressive. The tensile stresses at the outer surface are lower and occur at much lower temperatures so that the predicted fatigue life would be longer. Table I-14 is a summary of the combined stress in the fuel cladding for the Design Transient and for the maximum planned transients, both of which were discussed in Section 1.3.

The combined stresses shown in Tables I-13 and I-14 have been used to predict the low cycle fatigue life of the end plugs, segmenting plug, and the fuel cladding using the ASME Fatigue Curves. The results of this evaluation are shown in Table I-15. These results demonstrate that the fuel cladding opposite the fuel is a critical location in the fuel rod from a fatigue life standpoint. For the Design Transient condition, the predicted fatigue life in the fuel cladding is shown as 30 cycles. A predicted fatigue life of 30 design transients is considered adequate to meet the requirements of the SEFOR experimental program for the following reasons:

- (1) The fatigue curves given in Section III of the ASME Boiler and Pressure Vessel Code contains a margin of approximately an order of magnitude in the predicted cycles of failure, i.e., a more probable fatigue life for the cladding is 300 rather than 30 Design Transients.
- (2) The total number of prompt critical transients planned is less than 15. (See Reference 6)

Table I-14

MAXIMUM COMBINED STRESS IN THE FUEL CLADDING

<u>Transient</u>	<u>Maximum Combined Stress</u>
Design Transient	150,000 psi
Maximum Planned Transient	67,000 psi

Table I-15

PREDICTED FATIGUE LIFE FOR THE FUEL ROD

<u>Location</u>	<u>Transient</u>	<u>Predicted Cycles to Failure</u>
Bottom End Plug	Refueling	∞
	Design Transient	∞
Segmenting Plug	Refueling	$>10^6$
	Design Transient	250
Cladding Opposite Fuel	Refueling	$>10^6$
	Design Transient	30
	Maximum Planned Transient	750
Top End plug	Refueling	$>10^6$
	Design Transient	3500

- (3) The Design Transient (as discussed in Section 1.3) is considerably more severe than the largest planned prompt critical transient.

The importance of Item #3 above is shown by comparing the predicted fatigue life of the cladding for the Design Transient with that of the maximum planned transient. It is seen that the fatigue life of the cladding is increased from a predicted value of 30 cycles to failure for the Design Transient to 750 cycles to failure for the maximum planned transient. The increase in fatigue life at the other locations in the fuel rod have not been calculated for the maximum planned transient since the predicted fatigue life for the Design Transient at the other locations in the rod are already orders of magnitude larger than the predicted fatigue life of the cladding for the Design Transient.

1.5.3.6 Structural Evaluation of the Other Core Components

The structural evaluation of the fuel channel is discussed in detail in Section 1.4. Calculations were also performed on the side rods, spacer buttons, extension rods, tightener sleeve and attached hardware such as the springs and fins and the BeO tightener rod for the Design Transient. Particular emphasis was placed on evaluation of the attachment points (welds) for these components. The calculations followed the general procedure used in the evaluation of the fuel rod; namely, a heat transfer calculation to determine local temperatures and temperature gradients followed by a stress analysis using the thermal loadings determined from the heat transfer calculations. All of the predicted fatigue lives for these items were more than two orders of magnitude larger than that of the fuel rods themselves.

1.6 FUEL THERMAL PERFORMANCE

1.6.1 Introduction

The major objective of the SEFOR experimental program is to measure the Doppler coefficient in a fast reactor under both steady state and transient operating conditions for fuel temperatures in the range of interest for fast power reactors. Therefore, a major design requirement for the fuel is that it can operate under both steady state

and transient conditions at fuel temperatures up to the melting point of the fuel, and that these temperatures can be obtained at relatively low reactor core power density so that the total core power is limited to 20 MW_t . The high steady state fuel temperatures at the relatively low fuel power density were obtained by using substantially larger fuel rods than proposed for fast power reactors. Since SEFOR fuel rods are significantly larger in diameter than the proposed fuel rods for power reactors, an in-pile and out-of-pile fuel evaluation program was conducted as part of Task 3 of the Pre-Operational Research and Development Program. A summary of the in-pile results was given in Reference 9. The out-of-pile test work was reported in Reference 11. Results from these tests performed substantiated the design numbers that were selected for the SEFOR fuel. The following subsection contains a summary of the results of analyses performed to select the variables in the fuel design.

1.6.2 Steady State Fuel Temperatures

1.6.2.1 General

A basic description of the mechanical design of the SEFOR fuel rod is contained in Section II of this supplement. In addition to the standard fuel rod, there will be a small number of special fuel rods which differ from the standard fuel rod only in the percent of plutonium enrichment. These special rods referred to hereafter as "guinea pig rods" have a 25% fissile plutonium content as compared to the standard 18.7% fissile plutonium content of the standard fuel rods. Steady state temperature distributions in both the standard rods and the guinea pig rods are discussed in this subsection.

The mixed oxide fuel in the SEFOR core is fabricated into fuel pellets .875" in diameter and were prepared by cold pressing and sintering coprecipitated powder. Each fuel pellet is approximately 5/8" long and has been end-dished to reduce axial expansion as discussed in Section 1.6.5. Homogeneous fuel was achieved by the coprecipitation process which assures a near complete solid solution. In this fuel, the U-238 temperature follows the plutonium temperature

during any transient operation. This assures a prompt feedback from the Doppler effect during reactor transients.

1.6.2.2 Power Density Variation in the SEFOR Fuel

The relative power densities of a standard fuel rod in any position in the SEFOR core, is discussed in Section III of this report. Figure I-57 is taken from Section III and shows a two-dimensional contour plot of power density. The radial, axial and local power peaking factors are also discussed in Section III. The power peaking factors are used to determine the maximum power density in a fuel rod in the core (the "hot rod") to determine the margin between the maximum fuel temperature in the core and the melting point. Calculations were also performed at the average position in the core to determine fuel temperatures. Since as stated above, the major objective of the SEFOR experimental program is to measure the Doppler coefficient for fuel temperatures in the range of interest in fast reactors, it is desirable to operate the fuel as near the melting point as is safe. The guinea pig rods mentioned above have been incorporated in the operating philosophy of the reactor in an effort to approach the melting point of the fuel in a "bootstrapping" fashion, without risking a large number of fuel rods at the center of the core. These guinea pig rods will be located under ports in the reactor vessel head to facilitate ease of removal and inspection in order to examine these rods non-destructively as the power density is increased in a stepwise fashion.

Table I-16 is a summary of the locations available under thru-head ports in the reactor vessel head and the relative power level at these positions normalized to the standard hot rod at the center of the core. It will be noted in Table I-16 that the guinea pig rods located at the two basic radial positions span the operating range from just slightly below the normal hot rod power density to 15% above the normal hot rod power density. In this manner fuel which is readily inspectible can be operated at conditions which "lead" the standard fuel rods in the core a sufficient amount so that any incipient failure may be identified by the inspection facilities available in the hot cell above the reactor.

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Figure I-57. TWO-DIMENSIONAL CONTOUR PLOT OF POWER DENSITY (RELATIVE TO PEAK VALUE OF 0.295)

Table I-16

"GUINEA PIG" ROD RELATIVE POWER

<u>Radial Position</u>		<u>Relative Power</u>	
<u>Refueling Port</u>	<u>Rod Position</u>	<u>Normal Fuel Rod</u>	<u>Guinea Pig Rod</u>
7.8" R	7.31" R	0.863	1.153
	7.70" R	0.850	1.137
10.44" R	9.93" R	0.750	1.002
	10.50" R	0.721	.965

The power density in kilowatts per foot as a function of reactor power is shown on Figure I-58 for the cold rod, the normal hot rod, and the guinea pig rod (located at the innermost position under the refueling ports) all at core mid-plane. Also shown is the power density at the average core position. As can be seen from the figure, the average power density in the fuel at 20 MW_t rated power is 11.3 kw/ft. These curves are based on a nominal loading of 630 fuel rods using an active fuel length of 33.8 inch per rod. The effect of the variation in fuel loading (variation in number of fuel rods) has been investigated and the results are shown in Section 1.6.2.6.

1.6.2.3 Mixed Oxide Melting Point

The phase diagram for the UO₂-PuO₂ system determined by Lyon and Baily is applicable for the SEFOR fuel. Reference 14 describes the work by Lyon and Baily and includes a discussion of the experimental data substantiating this phase diagram. The results presented in Reference 14 are applicable for stoichiometric fuel. Information on the effect of stoichiometry on the melting point of mixed oxide is limited to the 20 weight % PuO₂ composition range with O/M varying from 1.93 to 2.00. These studies conducted under the AEC-funded Fast Ceramic Reactor Program at General Electric's Advanced Products Operation indicate a maximum in the melting point at approximately 1.98 stoichiometry for the 20 weight % PuO₂ composition. Figure I-59 is a plot showing the change in mixed oxide melting point with O/M ratio taken from Reference 14. Note that the maximum melting temperature of

Figure I-58. FUEL ROD POWER DENSITY AT VARIOUS CORE RADIAL POSITIONS

Figure I-59. MELTING TEMPERATURE OF $(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_{2-x}$

5200°F (2870°C) occurs near the 1.98 nominal stoichiometry for the SEFOR fuel. The curve shown on Figure I-59 is the liquidus temperature for the $\text{UO}_2\text{-PuO}_2$ system. As discussed in Reference 14, the difference in the solidus and liquidus temperatures for the 20% PuO_2 composition is approximately 50°F. For this reason, the temperature designated as the "melting point" for the SEFOR mixed oxide fuel has been conservatively reduced 100°F to 5100°F. An uncertainty band of $\pm 100^\circ\text{F}$ has also been assumed so that the minimum "melting" temperature used for evaluation of the SEFOR fuel is taken to be 5000°F. The 100° error band includes the estimated error in the measurement (given in Reference 14 as $\pm 20^\circ\text{C}$). Possible changes in the melting point due to changes in stoichiometry during the operation of the fuel have not been included. However, experimental data⁽¹²⁾ indicates that this effect tends to keep the fuel at the fuel rod center near the 1.98 stoichiometry so that the higher melting point is maintained. Because of the low burnup for the SEFOR fuel, changes in the fuel melting temperature due to burnup are not expected to be significant, and have not been considered in establishing a melting point for the fuel.

1.6.2.4 Mixed Oxide Fuel Thermal Conductivity

The integral form is a useful form of fuel thermal conductivity data for fuel design and evaluation purposes. The $\int K d\theta$ curve for SEFOR fuel has been generated by utilizing both in and out-of-pile data. Specifically, the integral conductivity from room temperature to approximately 3600°F is calculated from out-of-pile thermal conductivity data. The integral curve to fuel melting is then completed by adding integral to melting values calculated from in-pile experiment to this out-of-pile data. A summary of the state-of-the-art for fuel thermal conductivity of mixed oxide fuels is contained in Ref.12. The $\int K d\theta$ curve used in the evaluation of the SEFOR fuel is shown on Figure I-60. This curve has been corrected to the 92% theoretical density and for the stoichiometry of the SEFOR fuel. The integral from zero degrees centigrade to melting as shown on Figure I-60 is 86.9 watts/cm. The effect of changes in the $\int K d\theta$ on peak fuel temperature has also been investigated and is discussed in Section 1.6.2.6.

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Figure I-60. FUEL CONDUCTIVITY VERSUS TEMPERATURE

1.6.2.5 Steady State Center Fuel Temperature Calculations

For steady state operation, the center fuel temperature is of primary concern because the objective of the program is to operate the fuel at near center melting. Center fuel temperature can be easily and conveniently calculated using the standard integral calculation method. This method requires the $JKd\theta$ curve for the fuel (which was discussed in Section 1.6.2.4) and values for the fuel-to-clad and the clad-to-coolant heat transfer coefficients.

The heat transfer from the fuel clad surface to the coolant has been discussed in Section 1.2 under the steady state thermal hydraulic evaluation. It was pointed out in Section 1.2 that because of the low coolant velocity in the core, the major heat transport mechanism in the coolant is by a molecular conduction, with only a small heat transport contribution by eddy transfer. For this reason, only molecular conduction has been considered in determining heat transfer coefficients for the coolant. With this assumption, the average heat transfer coefficient for the SEFOR fuel rod has been calculated as $8400 \text{ BTU/hr-ft}^2\text{-}^\circ\text{F}$. This number is considerably lower than the $20,000\text{--}25,000 \text{ BTU/hr-ft}^2\text{-}^\circ\text{F}$ that is typical of clad-to-coolant heat transfer coefficients for sodium-cooled power reactors that are being proposed. However, because of the relatively low heat flux (approximately $300,000 \text{ BTU/hr-ft}^2$ maximum), the film drop between the cladding and the coolant is still relatively small for SEFOR.

Center fuel temperatures are more sensitive to the fuel-to-clad heat transfer coefficient. Typical values for fuel-to-clad heat transfer coefficients in gas-bonded mixed-oxide, stainless steel clad fuel range in the order of 500 to several thousand $\text{BTU/hr-ft}^2\text{-}^\circ\text{F}$. An important variable in the determination of the heat transfer coefficient is the gap between the fuel pellet and the clad. As discussed in Section 1.6.4 of this supplement, the design criteria for the fuel is that the gap between fuel and the cladding is nominally zero in the hot rod at the 20 MW_t operating condition.

Based on conduction through the gas between the fuel and the cladding for this condition, the nominal value for the heat transfer coefficient from the fuel to the clad in the normal hot rod (and the guinea pig rods) is calculated to be about $2000 \text{ BTU/hr-ft}^2\text{-}^\circ\text{F}$. Variations in the center fuel temperature as a function of magnitude of the fuel-to-clad heat transfer coefficient have been investigated and are discussed in Section 1.6.2.6.

A summary of the central fuel temperatures in the guinea pig rod and the normal hot rod as a function of reactor power is shown on Figure I-61. These calculations use the parameters and basic properties discussed above. The calculations also include a temperature correction due to central void formation that occurs as a result of densification of fuel near the center of the fuel rod. Densification to 100% theoretical is assumed to occur for all fuel operating at or above 3100°F . As shown on Figure I-61, the center temperature in the guinea pig rod at 20 MW_t is calculated to be 5000°F . This point (the minimum fuel melting temperature as discussed in Section 1.6.2.3) is a design point that was used to determine the enrichment for the guinea pig rod. With the nominal center temperature of the guinea pig rods at the melting point, there is reasonable assurance that the guinea pig rod can be used to investigate the entire operating range of interest for the rest of the core. The guinea pig rods will be examined periodically during the stepwise approach to the 20 MW_t operating condition to determine if and when an incipient failure point has occurred. As is discussed in Reference 9 (the summary of the fuel evaluation tests) melting, if it should occur, does not result in gross clad failure but rather manifests itself as a slight increase in the diameter of the fuel rod. Such behavior can be easily detected in the fuel dimensional check station available in the Refueling Cell above the reactor.

The variation in the peak fuel rod (hot rod) center temperature as a function of reactor power is also shown on Figure I-61. As indicated, the nominal peak centerline temperature at 20 MW_t is calculated to be 4500°F which is approximately 500°F below the minimum

Figure I-61. FUEL CENTER TEMPERATURES IN THE HOT RODS AND GUINEA PIG RODS

melting temperature for the mixed oxide fuel. This margin is believed to be adequate to cover the uncertainties in the various parameters that go into the calculations of the fuel temperature. (See further discussion below).

1.6.2.6 Sensitivity Study of Fuel Temperature Calculations

The sensitivity of the hot rod center fuel temperature to the fuel-to-clad gap coefficient is shown on Figure I-62. Shown on the figure are the hot rod fuel temperatures for three gap coefficients which are believed to bracket the actual value for the fuel rod. As can be seen in Figure I-62, a reduction in the fuel-to-clad gap coefficient from the expected value to $1000 \text{ BTU/hr-ft}^2\text{-}^\circ\text{F}$ results in a center fuel temperature increase of only about 100°F .

Figure I-63 shows the affect of loading on the center fuel temperature of the hot rod. As discussed in Section III, loading margin is provided in SEFOR by the use of poison rods containing B_4C . The nominal loading for the SEFOR core is 630 fuel rods and 18 poison rods. Therefore, the actual fuel loading can vary from a minimum of 612 rods to a maximum of 648 fuel rods. From Figure I-63 it can be seen that the maximum variation in loading results in a center fuel temperature change in the hot rod of approximately $\pm 50^\circ\text{F}$.

Calculations have also been performed to determine the sensitivity of the center fuel temperature in the hot rod to the $\int \text{Kd}\theta$ curve. Calculations have been performed in which the integral curve has been arbitrarily increased and decreased by 10%. This would correspond to a variation in the integral to melting of 78 to 96 watts/cm. As can be seen from Figure I-64, a $\pm 10\%$ change in the $\int \text{Kd}\theta$ results in $\pm 250^\circ\text{F}$ change in the center temperature of the hot rod. Variations in fuel density and enrichment introduce an additional uncertainty of 75°F .

The uncertainty in the center fuel temperature for the various parameters discussed above are viewed as 2σ type variations. If these variations are combined with the uncertainty in the power peaking factor, as discussed in Section III of this report, the resulting uncertainty in the nominal peak calculation temperature is about 380°F . Thus, the normal hot rod temperature would be expected to be $4500^\circ\text{F} \pm 380^\circ\text{F}$ with 2σ certainty.

Figure I-62. VARIATION OF THE HOT ROD CENTER FUEL
DUE TO VARIATIONS IN THE FUEL-TO-CLAD
HEAT TRANSFER COEFFICIENT

Figure I-63. EFFECT OF FUEL LOADING ON HOT ROD CENTER FUEL TEMPERATURE

Figure I-64. VARIATION OF HOT ROD CENTER FUEL TEMPERATURE WITH CHANGES IN $\int kd\theta$

1.6.3 Transient Fuel Temperatures

Maximum fuel temperatures for the design transient have already been discussed in Section 1.3.2.3. It was stated in that subsection that the maximum fuel temperature for the design transient is calculated to be 5100°F. This peak temperature contains a significant amount of conservatism because of the heat capacity value used in the calculations. Figure I-65 is a plot of the heat capacity as a function of temperature for UO_2 and some recent data for mixed oxide fuel having a composition and stoichiometry essentially the same as the SEFOR fuel. Since the data for mixed oxide only recently became available, the temperatures reported in the section of the transient analysis were based on a heat capacity of UO_2 which is shown as curve A on Figure I-65.

To interpret the effect of Curve B (the mixed oxide data), versus that used in the transient, two points should be made. First, the magnitude of the design transient is determined essentially by the Doppler reactivity feedback which is a function of the appropriate weighted average fuel temperature rise in the core during the transient. The second point is that the peak fuel temperature reached during the transient is a strong function of the heat capacity in the higher temperature regions. Referring to Figure I-65, it is seen that the heat capacity for UO_2 (which was used in the transient calculations) has a higher value up to the vicinity of 1400°K. The mixed oxide data is higher for temperatures beyond 1400°K. Since the Doppler reactivity feedback is a function of change in the average temperature, the higher heat capacity values shown in the range up to 1400°K are conservative since the temperature rise as a function of energy deposited would be less for the higher heat capacity curve. Since the average temperature for the core does not exceed about 1400°K for the design transient, it follows that the use of the UO_2 data gives a conservative upper limit on the total energy release from the Design Transient. Conversely, the use of the lower heat capacity at the higher temperatures gives a larger temperature rise in the hot rod for the given transient than would be obtained if the mixed oxide data were used.

Figure I-65. HEAT CAPACITY FOR UO₂ AND UO₂-PuO₂

The net effect is that the use of the UO_2 heat capacity curve has resulted in over-prediction of the total energy release as well as an over-prediction of the peak temperature in the center fuel rod (hot rod) in the core.

Even more significant than the conservative property values used is the fact that the planned transients for SEFOR are considerably smaller than the Design Transient discussed above which results in center fuel temperatures in the hot rod reaching the melting point. As discussed in Section 1.3.2.4 the maximum fuel temperature reached for the largest planned transient is approximately 3100°F . (Again, this number is an over-estimate because of the use of the UO_2 heat capacity data rather than the mixed oxide data.) It should be emphasized that the heat capacity data reported from the mixed oxide is based on incomplete work being performed at Los Alamos. Once these experiments have been completed and the results finalized, the transient program will be re-evaluated using the mixed oxide temperature dependent heat capacity relationship.

1.6.4 Fuel Pellet Radial Expansion

As mentioned in the previous section, the design criterion for the fuel rod is that the fuel-to-clad gap at hot operating conditions (20 MW_t) in the peak power density position in the core is zero. This design criterion was chosen to avoid excessive fuel clad interaction which could cause local cladding strain or result in gripping of the cladding by the fuel which could introduce reactivity anomalies due to axial change in the height of the core resulting from such interaction. Analyses have shown that this condition can be met if the original cold diametral gap between the fuel pellets and the cladding is 15 mils. That is, the fuel pellets are calculated to expand 15 mils more than fuel cladding going from room temperature to 20 MW_t operating temperatures. The fuel pellet expansion is calculated on the assumption that the fuel pellet cracks radially into pie-shaped segments so that the radial expansion is one dimensional and is simply the integral of the temperature distribution and the instantaneous coefficient of expansion. The adequacy of the 15 mil cold diametral gap has been demonstrated in the in-pile testing as reported in Reference 9.

These in-pile capsule tests showed that there was negligible (1 mil maximum) ridging at the pellet interfaces for initial cold diametral gap of 14 mils. Transient testing was also performed on capsules with the initial cold gap of 14 mils and shown to be satisfactory.

1.6.5 Fuel Column Axial Expansion

Preliminary calculations during the planning of the experimental program for SEFOR indicated that the axial expansion reactivity effect in SEFOR could be quite large and might make it difficult to accurately separate out the Doppler coefficient from the total prompt reactivity effect measured during the experiments. In order to get a more accurate Doppler coefficient measurement, the fuel columns in SEFOR were divided into two parts with the upper and lower column separated by a gas plenum. This design referred to as the segmented fuel design substantially decreases the axial fuel expansion reactivity effect and allows for much improved accuracy in the Doppler coefficient measurement. The reactivity effect of segmenting is discussed in Section III of this report.

Additional calculations indicated that the accuracy of this measurement could be improved further by reducing the amount of expansion of each of the two fuel columns by end-dishing the fuel pellets. Therefore, both ends of the fuel pellet are dished to a depth of 12 mils leaving a shoulder 40 mils wide at the pellet OD. A simple analytic model was developed to predict the axial expansion of a column of these end-dished fuel pellets. This model assumes that the axial expansion of the pellet column is a function of the temperature at the ID of the shoulder. Since thermal expansion data was not available for mixed oxide fuel, the isothermal expansion data for $UO_2^{(13)}$ is used for these calculations. Out-of-pile thermal expansion measurements on UO_2 were made to check the accuracy of this model and to study the reproducibility and stability of the dished fuel pellet. The results of this work are reported in Reference 11. The basic conclusions from this out-of-pile study are:

1. Dished pellets exhibited a definite reduction in axial expansion relative to undished pellets during steady state operation at a central temperature of 2000°C and a radial temperature gradient of approximately 3500°C/in. The dished pellet expanded approximately 40% less than the non-dished pellet.

2. Dished pellets showed good expansion stability and reproducibility under the test conditions and the expansion agreed well with the temperature at the inner shoulder of the dish. Utilization of this dish shoulder model provides a reliable basis for predicting the axial expansion of the fuel in SEFOR.
3. The diametral expansion of dished and non-dished pellets is similar and reproducible and independent of thermal cycling fuel melting and axial restraint.

Figure I-66 is taken from Reference 11 and shows the good reproducibility of the end-dished fuel expansion and the good agreement with the shoulder model. It should be noted that dishing is effective in reducing axial expansion only during steady state or slow transient operating conditions. For rapid transients (such as the Design Transient) the fuel temperature rise across the fuel pellet is essentially uniform and the dished fuel column would expand approximately the same as a fuel column of undished pellets. Adequate expansion space is provided for this condition for the Design Transient.

Figure I-66. THE AXIAL EXPANSION CHARACTERISTICS OF AXIALLY RESTRAINED DISH PELLETS UNDER A TEMPERATURE GRADIENT

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SECTION II2.0 FUEL MECHANICAL DESIGN2.1 DESIGN OBJECTIVES

The SEFOR core is designed to remain in service throughout the planned three-year R&D program, based on 20 MWt operation and a seven percent operating factor.

Major design objectives are based on safety and flexibility in the experimental program. These are:

- 2.1.1 Core volume changes are minimized for all operating conditions. This is important to improve the accuracy of the Doppler measurements, and is accomplished by core clamping and segmented fuel as described in subsequent sections.
- 2.1.2 The fuel rods are designed for individual removal and insertion to permit examination following each phase of the experimental program. Selected rods are removable without removing the vessel head. A mechanical tightening mechanism in each channel provides clearance for fuel insertion and removal during shutdown, and spring loads and confines the fuel rod in the intended position in the channel during operation.
- 2.1.3 All core components are designed for gravity hold-down to eliminate dependency on mechanical hold-down devices. The weight of any component exceeds the hydraulic forces by a safe margin.
- 2.1.4 Zoned orificing is provided to flatten the outlet coolant temperature. Orifices in individual channels are removable and replaceable.
- 2.1.5 Beryllium-oxide is remotely removable to permit Doppler measurements with and without BeO.

2.2 CORE ELEMENT DESCRIPTION

- 2.2.1 General: The SEFOR core structure is made up of 109 hexagonal tubular channels that are clamped around the periphery to form a bundle approximately 34.7 inches in diameter and 106 inches long.

The channels stand on a core support plate and each channel is attached to the support plate by an orifice assembly. The channels and orifices, although manually removable, are not removed for refueling or other routine servicing. A plan view of the core structure is shown in Figure II-1.

Each of the channels receives six fuel rods, one tightener rod, and a tightener sleeve. A channel and its internal components is called a core element. The core element is shown in Figure II-2.

The core element is assembled by first installing the tightener sleeve, then inserting the tightener rod in the sleeve. When the tightener rod is four inches above the fully inserted position, it is supported by six springs attached to the lower end of the tightener sleeve tubing. In this position, the tightener springs at four other elevations on the tightener sleeve align with reduced-diameter sections of the tightener rod. In this position, the tightener springs are free to swing inward toward the center line of the tightener sleeve, providing clearance for installation of the fuel rods. The fuel rods are inserted individually. When all six fuel rods are inserted, the tightener rod is pushed down the remaining four inches, forcing the tightener springs outward against the fuel rods and spring loading the fuel rods against support points on the channel inside wall. The tightener sleeve, tightener rod, and fuel rods are individually installed and removed remotely by the special crane-supported grapples.

2.2.2 Significant Data: Significant information on the core element components follows:

2.2.2.1 Core Dimensions

Equivalent Diameter	in.	34.7
Cumulative Fuel Length	in.	33.812
Core Height (including axial gap and UO ₂ insulators)	in.	36-9/16

2.2.2.2 Core Element

Maximum Number in Core		109
Calculated Weight	lb.	124 lb.
Fuel Rods per Core Element		6
Tightener Rods per Core Element		1
Rod Pitch	in.	1.095

2.2.2.3 Fuel Channel

Material		Type 304 SS
Shape		Hexagonal
Outside dimension across flats	in.	$3.150^{+}.003$
Wall thickness	in.	$0.060 \pm .002$
Tubing length	in.	105
Overall length	in.	106
Side rod diameter	in.	$.250 \pm .001$
Weight	lb.	22

2.2.2.4 Fuel Rod

Weight (excluding Extension Rod)	lb.	11.0
Overall length (standard rods)	in.	49-5/8
(special rods)		49-11/16
Diameter	in.	0.972
Fuel length	in.	33.812
Over-all core length (including insulator and void)	in.	36-9/16
Number of axial segments		2
Cladding		
Material	Type 316 SS	
Inside Diameter	in.	0.890 ± 0.0015
Wall	in.	0.041 ± 0.0025
Fuel Pellets		
Material	$\text{PuO}_2\text{-UO}_2$	
Diameter	in.	0.875 ± 0.001
Length	in.	$5/8 \pm 3/32$
Density (based on pellet OD)		
	% of theo.	92.6 ± 2
	gm/cc	10.2
Density (based on cladding ID)		
	% of theo.	89.5
	gm/cc	9.85
Composition	$\text{PuO}_2\text{-UO}_2$	
$\text{Pu}(239 \pm 241)/\text{Pu} + \text{U}$		
standard rods	%	18.7
special rods (Guinea Pig)	%	25.0

Insulator Pellets

Material	Depleted UO_2	
Diameter	in.	0.883 ± 0.001
Length	in.	3/8
Density		
	% of theo.	92
	gm/cc	10.1

Reflector (upper & lower)

Material	Nickel-ASTM-B160	
Diameter	in.	$0.875^{+0.0000}_{-0.007}$
Length	in.	3-31/32

2.2.2.5 Extension Rod

Overall length	in.	64-27/32
Weight		
Standard	lb.	4.12
Special	lb.	5.84
Cladding		
Material	Type 316 SS	
Outside diameter	in.	$.875 \pm 0.005$
Wall	in.	0.040 ± 0.005
OD at spring contact points	in.	0.970 ± 0.005

Shielding

Material		
Standard	B_4C	
Special	50% B_4C and 50% SST. shot	
Min. density of B_4C (standard rods)	gm/cc	1.7
Length	in.	54
Min. weight		
Standard	lb.	1.68
Special (contains steel shot)	lb.	3.7

2.2.2.6 Tightening Rod

Overall length	in.	111-27/32
Weight	lb.	10.0
Diameter	in.	0.875 $\pm$ 0.002
Cladding		
Material	Type 316 SS	
Outside Diameter	in.	0.875 $\pm$ 0.002
Wall	in.	0.040 $\pm$ 0.002
BeO Rod		
Material	Sintered BeO pellets	
Diameter	in.	0.777 ^{+0.000} _{-0.003}
Overall length	in.	36-9/16
Density	gm/cc	2.9
Shielding		
Material	50% B ₄ C plus 50% steel shot	
Min.effective density	gm/cc	3.75
Min. weight	lb.	3.9
Length	in.	56
Reflector		
Material	Nickel, ASTM B160	
Diameter	in.	0.780 ^{+0.005} _{-0.000}
Length (upper)	in.	3-31/32
Length (lower)	in.	3-31/32

2.2.2.7 Tightener Sleeve

Overall length	in.	110-1/8
Tube Material	Type 304 SS	
Spring material	Inconel 718	
Spider material	Type 304 casting	
Weight	lb.	5.3

2.2.2.8 Orifice

Bore diameter (central)	in.	1.550
G1	in.	1.296
G2	in.	1.280
G3	in.	1.096
G4	in.	1.018
G5	in.	.920

2.2.3 Fuel Rod

The SEFOR fuel rod is shown in Figure II-3. The fuel is divided into two segments to reduce the axial reactivity expansion coefficient by reducing the change in radial neutron leakage with changes in fuel temperature (steady state power level).

Fuel pellets are $\text{PuO}_2\text{-UO}_2$ prepared by cold-pressing and sintering co-precipitated powder. Both ends of each pellet column are insulated from supporting hardware by depleted UO_2 insulator pellets, also prepared by cold-pressing and sintering. The pellet columns are held in place by Inconel-X springs which apply a spring load to the pellets of 1 to 3 times the pellet column weight. Nickel reflectors are located above and below the fuel. Cladding is type 316 stainless steel and the segmenting spacer plug and end plugs are type 304 stainless steel. The segmenting plug is electron-beam welded to the tubing, forming a seal between the upper and lower segments. Top and bottom end plugs are fusion welded to the cladding. Gas space in the fuel rod is filled with Bureau of Mines Grade A helium.

2.2.4 Extension Rod

The extension rod serves the dual function of an upper shield and an extension to aid in fuel insertion and removal. The extension rod shielding material is vibratory-compacted boron carbide powder. A group of special extension rods contain 50% B_4C and 50% SST shot. Tubing is type 316 stainless steel and end plugs are type 304 stainless

steel. The gas space is back-filled with helium.

The tubing is expanded at two elevations corresponding to the support spacers in the channel to permit support and tightening. Diameter of the expanded tubing is .970 inch, which is the same as the fuel rod diameter. The remainder of the tubing is .875 inch to effect lower coolant velocity and pressure drop in the region above the core.

The lower end of the extension rod is connected to the fuel rod top end plug by a connector pin. The elevation of the reduced diameter area above the connector pin corresponds to the cross-flow hole elevation in the channels. The top end plug of the extension rod is shaped to receive the refueling grapple.

Each type of core rod (fuel, rod, poison rods, etc.) receives an extension rod bearing a special identification mark on the top end plug. The non-fuel rod extension rods are the special rods containing 50% B₄C and 50% SST shot. These special extension rods are used to increase the total weight of these non-fuel rods. The dimensions of the connector vary on the several types of core rods to prevent interchangeability. The extension rod top end plug is shown in Figure II-4.

2.2.5 Fuel Channel

The channel forms the outside container of the core element. It provides support points for the fuel rods at five elevations by means of spacers that are fixed to the inside wall of the channel by fusion welds. In the core region, a side rod is fixed to each channel flat by the spacers. The side rods serve to provide a more uniform flow area around the fuel rods.

An axial seal on the lower end of the channel limits bypass leakage. The sealing surface is spring-loaded against the core support plate (grid plate) by a bellows.

Coolant flow is individually orificed to each channel by an orifice piece which also serves to attach the fuel channel to the core support plate by a bayonet type lock. Radial clearance between

the orifice and the inside diameters of the fuel channel permit up to 1/8 inch lateral movement of the channel during core clamping.

Holes in each flat of the channel in the region above the core permit cross flow for coolant during low reactor vessel coolant level operation. Other holes in the channel flats near the top of the channel receive a special handling grapple.

2.2.6 Tightening Rod

The tightening rod serves three functions. When inserted in the central position of the core element, the tightening springs on the flats of the tightener sleeve are forced outward against the fuel rods, which are in turn forced outward against the spacers on the channel wall. The in-core portion of the tightening rod contains BeO pellets which serve to soften the neutron spectrum. The upper section of the tightening rod is filled with 50% B₄C powder and 50% stainless steel shot which serves as an axial shield. The shielding material and BeO are separated by a steel bulkhead in the tube. Gas space is back-filled with helium. The upper end of the tightening rod is shaped to receive the refueling grapple.

2.2.7 Tightener Sleeve

The tightener sleeve (Figure II-5) serves several functions. A special fitting at the lower end locks the orifice in place and supports the bottom end of the fuel rods at the proper location in the channel. Radial fins in the core and at the top of the channel center the tightener sleeves in the channel and ensure a minimum spacing between fuel rods at the fin elevations. The upper fins also serve as a visual reference during refueling. Tightener springs on each flat of the tightener sleeve at four elevations spring-load the fuel rods against the spacers on the channel wall. Springs are attached to the sleeve by spot welds and the fins by plug welds. The lower fitting is fusion welded to the tube. Holes in the tube at the top end receive a special grapple.

2.2.8 Special Core Rods

The experimental program requires several special types of rods which will be placed in fuel positions in the core.

2.2.8.1 Poison Core Rods

Rods filled with boron carbide are shown in Figure II-6. These rods are identical to fuel rods except the core region contains a measured amount of B_4C powder compacted to fifty percent of theoretical density and a steel rod to occupy the remaining volume. A segmenting plug at the top of the core prevents powder from entering the reflector region. These rods (and all special rods) are identified by a unique serial number on the bottom end plug. Top end plug dimensions permit only the correct type of extension rod to be attached.

2.2.8.2 Practice Core Rods

Practice core rods are shown in Figure II-7. These rods contain a stainless steel rod in the core and reflector regions.

2.2.8.3 UO₂ Core Rods

Uranium-dioxide core rods contain depleted UO₂ pellets in the core region as shown in Figure II-8.

2.2.8.4 Foil Holder Rods

Foil holder rods are shown in Figure II-9. These rods will contain foils used for physics measurements, and can be disassembled remotely for foil removal. These rods will only be used during wet critical testing.

2.3 MECHANICAL DESIGN EVALUATION

2.3.1 Use of Boron-Carbide Shielding

Boron-carbide was selected for the shielding because of its shielding properties, cost, and compatibility with the environment. Powder was selected over sintered pellets because it provided adequate shielding at a much lower cost. Technical grade, high boron content material is specified.

The B_4C will be at a temperature of 300°F-700°F for 24,000 hours during reactor shutdown and at a temperature of 920°F (central) and 820°F (surface) for an estimated 2000 hours during the three years of planned use. Tests have shown B_4C to be compatible

with 300 series stainless steels for 10,000 hours at 1100°F. The maximum allowable service temperature of B_4C in an inert atmosphere is 4100°F. Although no test data exists for the planned length of service, the 10,000-hour tests at a higher temperature indicate that no significant reaction should be expected.

If cladding should leak, allowing sodium to come into contact with the B_4C , no serious results would be expected. Boron-carbide does not swell when exposed to sodium, nor does it readily decompose. In work at Pratt and Whitney, fine grained sub-stoichiometric B_4C immersed in 1100°F sodium for 3000 hours was still intact. Based on these results, severe sodium contamination outside the rod would not be expected. The consequence of sodium contamination with carbon is carburization of high temperature metal surfaces, but carburization occurs slowly, or does not occur, at SEFOR fuel cladding temperatures (900°F).

Boron-carbide will react slowly with stainless steel in the presence of sodium at high temperature. Tests for 10,000 hours at 1000°F resulted in reaction products penetrating the cladding 0.009 inch and non-metallic diffusion zones 0.001 to 0.002 inch thick. Since the extension rod cladding is 0.040 inch thick and stresses are low, this type of reaction in the event of a leak would not likely lead to cladding failure to the extent that B_4C would be released into the coolant. Frequent samples of the SEFOR coolant will be checked for carbon level.

Boron carbide has been used as a control material in numerous water reactors and in the SIR sodium-cooled reactor.

Helium gas is formed by neutron capture in B_4C . Approximately 5 cubic inches (S.T.P.) of helium will be produced in the peak rod during the planned rod life. Available gas space for 70% dense powder is 8 cubic inches. The gas pressure, even assuming 100% release, would be negligible. (~ 0.6 atm).

2.3.2 Fuel Channel Design

The fuel channel is designed to withstand external clamping loads with negligible deflection from its hexagonal cross-section, and stresses due to internal pressure (see Section 1.4). The bellows seal at the lower end is designed to limit bypass leakage. The bellows is designed for service throughout the planned lifetime of Cores I and II.

The minimum face load on the bellows seal is 3 pounds and the area of the bellows and seal ring is sized so that internal pressure increases the face load on the seal ring. Failure of a seal would result in increased bypass leakage, but would not starve the individual channel. The increased leakage would tend to reduce the total sodium flow through the core.

2.3.3 Beryllium-Oxide

The beryllium-oxide pellets are contained inside the stainless steel tightener rod tubing. In the design, adequate space was allowed for BeO swelling due to irradiation without diametral or axial interference with the cladding or end plugs. Adequate space was also provided for the helium gas produced by irradiation.

The peak fast neutron dose ($E > 1 \text{ Mev}$) in the BeO during Core I and II operation is 6×10^{20} nvt. BeO irradiation data reported by the Nuclear Materials and Propulsion Operation after extensive irradiation testing showed that about 2 percent diametral and axial swelling can be expected. Diametral and axial gaps were sized to accomodate this amount. There would be no serious consequences if a cladding failure allowed sodium to contact the BeO. In tests at 1500°F, static Na did not react with BeO⁽¹⁾.

2.3.4 Welding

All closures on fuel, BeO and B_4C filled rods are fusion welds. The weld specified is designed to minimize transient thermal stresses due to unequal cross-sections. The weld is made at the junction of the cladding and the conical skirt extending from the end plug (Fig. II-10). The two mating surfaces are of equal thickness. The weld setup is made with a .005 inch clearance between the mating surfaces of the end plug and cladding to provide a passage for

venting the internal He that is expanded during the weld operation. The specified minimum penetration corresponds to 93% of the wall thickness of the cladding.

Rigid quality control measures were adhered to during fabrication to ensure uniform weld quality. Inspection methods include X-ray and metallographic sections of weld joints.

2.3.5 Stainless Steel

Stainless steel was selected for all tubing and end plugs in the core element because of availability, cost, and demonstrated reliable performance in similar applications and extensive tests approximating SEFOR conditions ⁽²⁾. Tubing used for cladding is Type 316 stainless steel because of its higher strength. Type 304 stainless steel is used for end plugs and channel tubing because of the lower strength requirements for these items. Also, improved welding characteristics are provided by joining Type 304 end plugs to type 316 tubing. In some applications where type 316 stainless steel pieces are joined by welding while restrained, microscopic cracking has occurred due to the hot-short characteristics of the weld during cooling. The high ferrite content of the type 304 end plug material reduces the probability of such cracking.

2.3.6 Inconel-X Springs

Inconel-X was selected for spring materials in the fission gas space of the fuel rods and BeO rods because of its high temperature strength and reasonably good resistance to irradiation. The springs are provided to apply a force to the fuel stack during handling prior to irradiation to eliminate motion of the pellets relative to the cladding. Although the springs are expected to improve the reproducibility of the axial fuel expansion during irradiation, the mechanical design and physics calculations do not rely on this occurring.

The nominal as-fabricated spring force is twice the weight of the supported column. The corresponding spring stress is 46,000 psi for the upper spring and 60,000 psi for the lower spring. Yield strength for the Inconel-X is greater than 100,000 psi.

2.3.7 Fuel Rod Motion

The total possible fuel rod lateral motion under worst-tolerance conditions is less than 1/16 inch. The rod motion is limited by rigid spacers on the channel wall and dimples on the fins and the tightener sleeve. Forces exerted by fuel rod bowing gradients are not strong enough to cause measurable deflection of these components.

Since there are no control rods in the reactor core, deformation of the core components cannot affect the proper functioning of the reactor control system.

Horizontal seismic loads of one g produce negligible stresses in core element components.

2.3.8 Inconel-718 Springs

Inconel-718 is used for the fuel channel bellows seal and the tightener springs which load the fuel rods against the channel spacers. The material was preferred over Inconel-X because of its better weldability. Limited irradiation data indicate that it is also equal to or superior to Inconel-X in irradiation environments.

Maximum extreme-fiber stress in the bellows seal is 63,000 psi and operating temperature is 700°F. The yield strength is 125,000 psi at 800°F. Irradiation dose on the bellows will be about 2×10^{19} nvt ($E > 1$ Mev) during Cores I and II. Inconel-718 irradiated to 4.6×10^{20} in 750°F water showed little change in yield strength, tensile strength, or ductility ⁽³⁾.

Stress rupture is not an important consideration in 200°F - 850°F service. Tables on stress rupture for Inconel-X and Inconel-718 start at 1000°F, and the 10,000 hour strength at this temperature is essentially equal to the yield strength ⁽⁴⁾.

Maximum stress for the tightener springs when fully compressed is 41,000 psi and the steady state temperature is 820°F. The irradiation dose is 6×10^{20} during Cores I and II. The Inconel-718 test results above indicate the material suitability for these conditions.

Fracture of both leaves of the spring is necessary for any part of the spring to separate from the tightener sleeve. The spring still serves as a stop to limit fuel rod motion with either leaf fractured.

Core vibration tests demonstrated that fuel rods would vibrate less than .002 inch due to rated coolant flow with half of the tightener springs completely missing.(5)

REFERENCES

SECTION II

- (1) W. H. Cook, "Corrosion Resistance of Various Ceramics and Cermets to Liquid Metals", Report No. ORNL-2391 (June 15, 1960).
- (2) J. H. Field, et al., "An Evaluation of the Effect of Design and Operating Variables on SEFOR Fuel and Fuel Cladding", GEAP-5309, December 1967.
- (3) Letter T. T. Claudson (BNWL) to R. E. Wallace (GE-APO), dated March 22, 1966.
- (4) Current Data Report, Inconel-718, Huntington Alloy Products Division, The International Nickel Company, Inc.
- (5) E. P. Quinn, "Vibration of SEFOR Fuel Rods in Parallel Flow", GEAP-4966, Sept., 1965.

Figures for Section II

GEAP-13598

FIGURE II-1

[illegible]

FIRST MADE FOR TIGHTENING ROD

112C2072

UNLESS OTHERWISE SPECIFIED USE THE FOLLOWING -			
APPLIED PRACTICES	SURFACES	DIMENSIONS IN INCHES	
		MIN. DIM.	MAX. DIM.
145A5481	125 ✓	1/64	.003

APPLIED PRACTICES
145A5481

SURFACES
125

1. 1/64

SALES	100.00
EXPENSES	10.00
NET	90.00

$$23 \div 2 = 11 \text{ R } 1$$

1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16	17	18	19	20	21	22	23	24	25	26	27	28	29	30	31	32	33	34	35	36	37	38	39	40	41	42	43	44	45	46	47	48	49	50	51	52	53	54	55	56	57	58	59	60	61	62	63	64	65	66	67	68	69	70	71	72	73	74	75	76	77	78	79	80	81	82	83	84	85	86	87	88	89	90	91	92	93	94	95	96	97	98	99	100
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GEAP-13598

[illegible]

NAME AL SCHNEIDER	ADDRESS 117 CIBOLA	CITY SUNNYVALE, CALIF.	STATE CALIF.
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FIGURE II-4

FIGURE II-5

GENERAL ELECTRIC

112C3891

UNLESS OTHERWISE SPECIFIED USE THE FOLLOWING -

APPLIED PRACTICES 145A543-1 SURFACES $\checkmark$ FRACTIONS $\frac{1}{16}$ DECIMALS $\frac{1}{16}$ ANGLES $\frac{1}{16}$

FCF191X658

112C3891

POISON CORE ROD

FIRST MADE FOR SEFOR R&D TASK 9.0

DRAWING NO. DESCRIPTION MATERIAL WEIGHT

REV	NO	DATE	DESCRIPTION	MATERIAL	WEIGHT
1	1		TUBE	159A 2747 P1	
1	2		REFLECTOR	159A 2751 P1	
1	3		REFLECTOR	159A 2751 P2	
1	4		SPRING	159A 2752 P1	
1	5		SPACER	159A 2754 P1	
1	6		ROD	175A 8593 P1	
AR	7		BORON CARBIDE	A 50 Y P3	
1	8		TOP END PLUG	129B 2357 P3	
1	9		BOTTOM END PLUG	129B 2359 P2	
2	3		10 SPIROL PIN	CAT NO 125-0750 LDP CEM CO, INC	
				OR ENGRG APPROVED EQUIVALENT	
1	11		SPACER	175A 9913 P1	
AR	12		FILLER	STAINLESS STEEL WOOL - FINE MESH	
3	13		ROD AND TUBES	135C 4906 FIG 3	
4	14		DRAWING LIST	194 X 78 4

DETAIL 15
ENLARGED

NOTES:

1. FABRICATION AND QUALITY CONTROL PRACTICES SHALL CONFORM WITH FUEL ROD SPEC 22A1548 SECTIONS 3, 1, 3, 3, 4, 1, 4, 2, 4, 4, 4, 6, 4, 7, 5, 0, 6, 1 & 6, 2.

2. ASSEMBLY TO BE STRAIGHT WITHIN .005/FT

3. WELD SHALL BE FULL PENETRATION AND HAVE AN OUTSIDE DIA OF .980 MAX.

4. WELD SPACER (P5) USING ELECTRON BEAM WELD.

5. INSERT ROD (P6) (WITH SPIROL PINS (P10) CENTERED IN P6)

6. INTO TUBE (P1) & FILL WITH 312 ± 1 GRAMS OF BORON CARBIDE (P7). VIBRATORY COMPACT TO LEVEL SHOWN. BORON CARBIDE POWDER SHALL BE A SPECIAL BLEND OF SIZES OR A SINGLE SIZE DESIGNED FOR COMPACTION TO 50% OF THEORETICAL DENSITY.

7. BEFORE INSTALLING END PLUGS (P8 & P9) EVACUATE TO 500 MICRONS OR LESS, BACKFILL WITH BUREAU OF MINES GRADE A HELIUM TO ONE ATMOSPHERE. INSTALL END PLUGS & WELD.

8. ENGRAVE SERIAL No. 010DP ± 5/16 HIGH CENTERED ON ONE END OF SLOT. LINE WIDTH 3/64. SERIALIZE USING NUMBERS BETWEEN X21 & X59.

(G) SEE NOTES 1, 2 & 6

DESCRIPTION OF GROUPS	REVISIONS	PRINTS TO
40.1/16 ± 0.001	1. 1/1/67	
AS BUILT	2. 1/1/67	
3. 1/1/67	3. 1/1/67	
ADDED P. 14	4. 1/1/67	
ADDED P. 14	5. 1/1/67	
ADDED P. 14	6. 1/1/67	
ADDED P. 14	7. 1/1/67	
ADDED P. 14	8. 1/1/67	
ADDED P. 14	9. 1/1/67	
ADDED P. 14	10. 1/1/67	
ADDED P. 14	11. 1/1/67	
ADDED P. 14	12. 1/1/67	
ADDED P. 14	13. 1/1/67	
ADDED P. 14	14. 1/1/67	
ADDED P. 14	15. 1/1/67	
ADDED P. 14	16. 1/1/67	
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ADDED P. 14	77. 1/1/67	
ADDED P. 14	78. 1/1/67	
ADDED P. 14	79. 1/1/67	
ADDED P. 14	80. 1/1/67	
ADDED P. 14	81. 1/1/67	
ADDED P. 14	82. 1/1/67	
ADDED P. 14	83. 1/1/67	
ADDED P. 14	84. 1/1/67	
ADDED P. 14	85. 1/1/67	
ADDED P. 14	86. 1/1/67	
ADDED P. 14	87. 1/1/67	
ADDED P. 14	88. 1/1/67	
ADDED P. 14	89. 1/1/67	
ADDED P. 14	90. 1/1/67	
ADDED P. 14	91. 1/1/67	
ADDED P. 14	92. 1/1/67	
ADDED P. 14	93. 1/1/67	
ADDED P. 14	94. 1/1/67	
ADDED P. 14	95. 1/1/67	
ADDED P. 14	96. 1/1/67	
ADDED P. 14	97. 1/1/67	
ADDED P. 14	98. 1/1/67	
ADDED P. 14	99. 1/1/67	
ADDED P. 14	100. 1/1/67	

FIGURE II-6

GEAP-13598

FIGURE II-7

GEAP-13598

GENERAL ELECTRIC

117C1364

UNLESS OTHERWISE SPECIFIED USE THE FOLLOWING			
APPLIED PRACTICES	SURFACES	FRACTIONS	DECIMALS
145A5481	✓	$\frac{1}{16}$	—

117C1364

TITLE
UO₂ CORE ROD
 FIRST MADE FOR **SEFOR R10** TASK 9.0

REV	BY	DATE	PART NO	NAME	DRAWING NO., DESCRIPTION, MATERIAL, WEIGHT
1	1	129 B	2357 P4	TOP END PLUG	129 B 2357 P4
1	2	159 A	2747 P1	TUBE	159 A 2747 P1
1	3	129 B	2359 P1	BOTTOM END PLUG	129 B 2359 P1
1	4	159 A	2752 P1	SPRING	159 A 2752 P1
AR	5	175 A	0597 P1	UO ₂ PELLET	175 A 0597 P1
2	6	159 A	2751 P1	REFLECTOR	159 A 2751 P1
1	7	175 A	0594 P1	SPACER	175 A 0594 P1
X	8	135 C	4906 FIG. 5	CORE ROD-EXTENDING ROD MATHS. 2314	135 C 4906 FIG. 5
X	9	194 X	786	DRAWING LIST	194 X 786

FCF 191 X 658

C*APPLIES FOR THIS DISTANCE.

(G) SEE NOTES 1 & 2

NOTES:

1. FABRICATION AND QUALITY CONTROL PRACTICES SHALL CONFORM WITH FUEL ROD SPEC. 22A154B SECTIONS 3.1, 3.3, 4.1, 4.2, 4.4, 4.5 (EXCEPT OMIT INITIAL 1 MICRON EVACUATION), 4.6, 4.7, 5.0, 6.1 & 6.2.

2. ASSEMBLY TO BE STRAIGHT WITHIN .005/FT.

3. WELD SHALL BE FULL PENETRATION AND HAVE AN OUTSIDE DIA. OF .980 MAX.

4. ENGRAVE SERIAL No. .010 DP $\times \frac{5}{16}$ HIGH CENTERED ON ONE END OF SLOT. LINE WIDTH $\frac{3}{64}$. SERIALIZE USING LETTERS BETWEEN DA AND DZ.

DESCRIPTION OF GROUPS	REVISIONS	PRINTS TO
	1 2.24.11.12.1 B.010 - 2.24.11.12.1 ADDED NOTE 4 2.24.11.12.1	
	2 1.2.24.11.12.1 ADDED PT. NO. B 2.24.11.12.1	
	3 1.2.24.11.12.1 ADDED P9	
MADE BY: <i>R. J. ...</i> 2.24.11.12.1	APPROVED: <i>A. P. O.</i> 2.24.11.12.1	BY: <i>...</i> 2.24.11.12.1
117C1364	117C1364	117C1364

FIGURE II-8

GEAP-13598

GEAP-13598

FIGURE 11

WELD

WELD PREPARATION

FUEL ROD

FIGURE II-10
FUEL ROD WELD PREPARATION

SECTION III

NUCLEAR ANALYSIS

3.1 CRITICALITY AND PLUTONIUM ATOM FRACTION

3.1.1 Results

The design atom fraction of $\text{Pu}^{239} + \text{Pu}^{241}$ for the SEFOR 2-segment fuel design is 18.7%. Values are listed in Table 3-1 for the atom percent and total inventory of all of the fuel isotopes in the SEFOR fuel. The inventory values are based on 648 fuel rods, i.e. the maximum number of fuel rods that can be loaded. In the actual loading, it is planned to replace some of these rods with B_4C rods (see Section 3.8.2). It was assumed that the Pu^{240} was 8.5% of the total plutonium. The values for Pu^{238} and Pu^{242} and the ratio of Pu^{241} to Pu^{239} represent typical values for 8.5% Pu^{240} fuel. The Pu^{241} concentration was sufficiently low that it was not distinguished from Pu^{239} in the design analysis.

TABLE 3-1

FUEL COMPOSITION

<u>Isotope</u>	<u>Atom %</u>	<u>Mass (kg)</u>
Pu^{238}	~.003	0.06
Pu^{239}	18.56	353.0
Pu^{240}	1.74	33.1
Pu^{241}	0.14	2.7
Pu^{242}	~.007	.1
U^{235}	0.17	3.2
U^{238}	79.83	<u>1510.0</u>
Total Pu		389.0
Total U + Pu		1902.2

3.1.2 Method for Establishing Plutonium Atom Fraction

The plutonium atom fraction was based on calculations normalized to critical mass calculations of the SEFOR mockup in ZPR-3.⁽¹⁾ The procedure for establishing the plutonium atom fraction included the following steps:

1. Establishment of the design criticality factor for 20 MW operation for SEFOR.
2. Calculation of criticality factors for the SEFOR mockup and for SEFOR using identical methods.
3. Normalization of the calculation of the SEFOR criticality factor by the ratio of calculated to experimental criticality factors for the SEFOR mockup.
4. Adjustment of the SEFOR plutonium atom fraction to obtain the design criticality factor.

Criticality calculations were made using diffusion theory using both 4-group, 2-dimensional (r,z) and 18-group, 1-dimensional (both radial and axial) calculations. Four-group and 18-group cross sections were condensed from 60-group cross sections in each important region using 60-group, 1-dimensional calculations in both the radial and axial directions. Group dependent transverse bucklings were used for all 1-dimensional calculations. The PDQ code was used for 2-dimensional calculations; the DEMON code was used for 1-dimensional calculations.

3.1.3 Design Criticality Factor for SEFOR

The design criticality factor for SEFOR operation at 20 MW must be greater than unity for two reasons. First, a reasonable margin of error must be allowed for the extrapolation from the mockup in ZPR-3 to SEFOR at 20 MW. It was estimated that a margin of 1.0 to 2.0% should be allowed for overestimating the criticality factor at 20 MW of Core (Section 3.1.6). Second, an allowance must be made for the reactivity loss on changing to the Core II design. Although the specific design for Core II is not fixed, various designs being considered involve removal of the beryllium oxide with an accompanying loss in reactivity in the range of 1.5% to 2.5%.

These considerations led to the choice of a design criticality factor of $k = 1.035$ at 20 MW.

3.1.4 SEFOR Mockup Criticality Calculations

The SEFOR mockup criticality calculations are described in detail in Reference 2. These calculations were made for the 1-segment, 2-segment, and 3-segment SEFOR mockups. The results of the 2-dimensional calculations for the critical loadings are given in Table 3-2. One-dimensional axial calculations are included for the 1- and 2-segment mockups. The calculated criticality factors should be compared with experimental values of unity.

TABLE 3-2

CALCULATED CRITICALITY FACTORS FOR MOCKUP
 (Calculated values are to be compared
 with experimental values of unity.)

<u>Loading</u>	<u>Calculational Model</u>	<u>Calculated Criticality Factor</u>	
		<u>2-D, 4-group</u>	<u>1-D (axial) 18-group</u>
1-Segment	2-dimensional (r,z), 4-group	0.985	0.989
2-Segment	2-dimensional (r,z), 4-group	0.978	0.985
3-Segment	2-dimensional (r,z), 4-group	0.974	

Although self-shielding at low energy was accounted for, no corrections were made for high energy heterogeneity effects in either the mockup or the SEFOR criticality calculations. The 0.985 value for the 1-segment core is consistent with those calculations of the initial SEFOR mockup loading quoted in Reference 3 which are based on the same cross section data used in the SEFOR program. A new set of cross sections is recommended in Reference 3 which gives much closer agreement with the initial loading.

Based on the results of Table 3-2, it was concluded that a normalizing factor between calculation and the SEFOR mockup experiment of 1.5% in k should be added to the calculated SEFOR criticality factors.

3.1.5 SEFOR 20 MW Criticality Calculations

3.1.5.1 Pu Atom Fraction Calculation

Using the same techniques and the same cross section data, criticality calculations were made for the final 2-segment SEFOR Core I design, for 20 MW operation. For these calculations, the $\text{Pu}^{239} + \text{Pu}^{241}$ atom fraction was 18.7%. The 4-group, 2-dimensional calculated criticality factor was 1.019; the 18-group, 1-dimensional axial criticality factor was 1.020. Using the 1.5% normalizing factors gives the design criticality factor of 1.035 for the 18.7% $\text{Pu}^{239} + \text{Pu}^{241}$ atom fraction.

The final 1-dimensional, 18-group axial SEFOR calculation was DEMON case C4A01. The final 1-dimensional, 18-group radial calculation was DEMON case C4C01. The 18-group cross section file used for the calculations was A18C4. This file was condensed by region from the 60-group file 60C4M.

U^{238} cross sections in SEFOR were calculated for the average fuel temperature at 20 MW, 1400°K, using the same techniques used for the room temperature mockup in ZPR-3. For Pu^{239} , room temperature cross section were used in the 20 MW SEFOR calculations since the ZPR-3 experiments verified earlier experiments which showed almost zero Doppler contribution from Pu^{239} .

3.1.5.2 20 MW Geometry and Compositions

The general method and principal assumptions used in obtaining the 20 MW core geometry and atom densities are outlined in this section. The details of the calculations are given in Appendix A.

The SEFOR repeating cell, as fabricated at room temperature, is shown in Figure 3-25, including all volume fractions. A 10 mil gap between channels was assumed for the clamped core at all temperatures and power levels. Atom densities were calculated at room temperature (except for sodium) using densities and volume fractions almost identical to those shown in Figure 3-25. Small differences between values in Figure 3-25 and those used in the nuclear calculations are discussed in Appendix A.

Atom densities were based on room temperature coefficients to account for changes with temperature. The room temperature atom densities were reduced at high temperature due to radial expansion of the structure and axial expansion of the specific material, according to the relation:

$$N_x(T) = N_x(\text{room temp.}) \frac{1}{(1 + \alpha \Delta T_{\text{structure}})^2 (1 + \alpha \Delta T_x)}$$

Values for $\alpha \Delta T$ of each material for the change from room temperature to 20 MW is given in Appendix A. Isotopic compositions of the fuel and material compositions of stainless steel 316 and 304 are given in Appendix A.

The sodium atom densities were calculated using temperature dependent densities and volume fraction listed in Appendix A.

3.1.6 Uncertainties in Criticality Calculations

Numerous differences exist between the SEFOR Mockup in ZPR-3 and SEFOR which introduce uncertainties in the SEFOR criticality calculations. A number of these uncertainties are identified and estimated below. The estimated errors are intended to have the significance of a standard deviation, meaning that it is expected that the true value has ~ 2 chances out of 3 of lying within the error range. The propagated value of all the errors listed below is $\pm 1.7\% \Delta k/k$.

(a). Uncertainties in Pu Composition: A 1 kg (out of 335 kg) uncertainty in fissile Pu would result in a $\pm 0.2\% \Delta k/k$ uncertainty in k . The fuel specifications allow for a 1 kg "tolerance" in total fissile fuel (i.e. 0.05 out of 18.7% fissile Pu atom fraction) plus a ± 1 kg "uncertainty" (i.e. another ± 0.05 out of 18.7%). The deviation from 18.7% due to "tolerance" will be known at the time of fuel delivery and reactor operation, but it was not known at the time of the specification of the Pu atom fraction and design criticality factor--which is the status being described in the present report. In addition, there may be a small difference in total fuel mass of 0.1% of the mass (or ± 0.3 kg fissile Pu). The total uncertainty in fissile Pu mass, therefore, is ± 2 kg, which leads to an uncertainty in k of $\pm 0.4\% \Delta k/k$.

(b). Uncertainties in Geometry and Composition: There are inevitably numerous small uncertainties in dimensions and compositions between the model used for criticality calculations and the actual reactor.

The outer boundary of the SEFOR core is more irregular than was the SEFOR mockup in ZPR-3. In SEFOR this boundary was treated by homogenizing an annulus of 52.4% core and 47.6% sodium-steel, 6.678 cm thick. In the ZPR-3 calculations, an effective core radius was used. This is the largest identified source of error due to geometry.

There will be some uncertainty in the space between fuel channels. A constant space of 10 mils was assumed, based on core clamping experiments made during the SEFOR research and development program. An increase of 1 mil in this spacing between each channel would increase the radius by 6 mils and would reduce the criticality factor by 0.024% $\Delta k/k$.

Uncertainties due to tolerance buildup are expected to be low. If the distance across flats of all channels was off the maximum tolerance of 3 mils in the same direction, the error in k would be 0.07% $\Delta k/k$.

The small radial variation in core height and resulting fuel atom density at high power in SEFOR was neglected, which will introduce some uncertainty in k .

Although most of the identified sources of error due to uncertainties in geometry and composition (other than fissile Pu) are small, a relatively large uncertainty of $\pm 1\%$ $\Delta k/k$ is attributed to this "catch-all" type error.

(c). Changes between the SEFOR Mockup and SEFOR: The largest and most important change in the SEFOR design which was made after the ZPR-3 critical experiment was the replacement of the BeO side rods by steel side rods. This resulted in a reduction in BeO volume fraction from 0.086 to 0.057. This change affects both the neutron spectrum and the Be(n,2n) effect. The total change in criticality factor due to this reduction in BeO was a reduction of 1.2% $\Delta k/k$ of which $\sim 0.34\%$ $\Delta k/k$ resulted from a decrease in the BeO(n,2n) source. The uncertainty in the extrapolation to SEFOR caused by this change in BeO was assumed to be 0.3% $\Delta k/k$.

A smaller change in SEFOR made after the ZPR-3 experiments was the addition of steel spacer fins to reduce bowing. The calculated reactivity penalty resulting from these spacer fins was only 0.07% $\Delta k/k$. The volume fraction of these fins was 0.0035. Half of the change in reactivity was due to the addition of the steel fins and half was due to the reduction in sodium volume.

(d). Temperature Coefficient: SEFOR was designed for a particular criticality factor at 20 MW whereas the SEFOR Mockup in ZPR-3 was assembled at room temperature. The calculated Doppler reactivity from room temperature to 20 MW was $\sim 4.1\%$, or $1.3\% \Delta k/k$. The total uncertainty on the extrapolation from the SEFOR Mockup to SEFOR associated with these temperature effects is $\pm 0.5\% \Delta k/k$.

(e). Neutron Leakage: The leakage probability from SEFOR is large, i.e. 34% (see Table 3-11, Section 3.4). Diffusion theory is inaccurate for the leakage calculation; its use was justified by using it for the analysis of both SEFOR and the SEFOR Mockup in ZPR-3. However, the difference in geometry between the fuel plates of ZPR-3 and the fuel rods of SEFOR will result in some difference in leakage between the two cases not accounted for by the homogenized diffusion theory calculations. This uncertainty was assumed to be $\pm 1.0\% \Delta k/k$.

(f). Differences in Heterogeneity: Self-multiplication by high energy neutrons in the ZPR-3 fuel plates increases the criticality factor by a value on the order of 1%. Some self-multiplication will also be present in SEFOR. The SEFOR fuel rods are larger than the ZPR-3 fuel plates which would tend to increase the self-multiplication. However, unlike the ZPR-3 plates, the SEFOR rods contain oxygen which would tend to decrease the self-multiplication. Any net difference of this effect between ZPR-3 and SEFOR was neglected in extrapolation to SEFOR. The error associated with neglecting this effect is assumed to be $\pm 0.5\% \Delta k/k$.

A second heterogeneity effect concerns the influence on the effective potential scattering cross section, σ_p , which affects the heavy isotope cross sections. The use of the calculated heterogeneous σ_p values increased the SEFOR criticality factor by 1.0% over that obtained with homogeneous σ_p values (see Table 3-14 in Section 3.5.7). The calculated self-shielding for the ZPR-3 plate geometry was very close to that for the SEFOR rod geometry. Since

cross sections were calculated for both geometries using the same technique, and since the self-shielding was nearly equal, the uncertainty due to differences between the SEFOR mockup and SEFOR were assumed to be small, i.e. $\sim 0.3\% \Delta k/k$.

(g). Propagated Uncertainty: The propagated uncertainty due to the effects described above is $\pm 1.7\% \Delta k/k$, assuming that these uncertainties are random and uncorrelated.

3.2 POWER DISTRIBUTIONS

3.2.1 Power Parameters

Specific power and power density are listed in Table 3-3. The fraction of fissions in each major fuel isotope is listed in Table 3-4. The distribution of fission energy among fission products and gamma and neutron energy is given in Table 3-5. The energy absorption in the core by material is given in Table 3-6.

TABLE 3-3

SPECIFIC POWER AND POWER DENSITY

Specific Power

Average	10.5 kw/kg (U+Pu)
Peak	19.1 kw/kg (U+Pu)

Average Power Density 37.7 kw/liter

TABLE 3 -4

FRACTION FISSIONS BY ISOTOPE

Pu-239	0.86
Pu-240	0.02
U-235	0.01
U-238	0.11

TABLE 3-5

ENERGY PRODUCTION IN CORE

<u>Source</u>	<u>Energy/Fission (MeV)</u>
Kinetic Energy of Fission Products	172
Kinetic Energy of Neutrons { Elastic Scattering Inelastic Scattering γ 's Neutron Leakage	5.7 { 1.7 2.8 1.2
Beta	9
Gamma (other than inelastic scattering γ 's):	
Prompt Fission	7.8
Fission Products	7.2
n, γ Reactions in Core	6.9
Alpha (for B ₄ C Rods Worth 3.5% $\Delta k/k$)	<u>0.13</u>
Total	209

TABLE 3-6

ENERGY LOSS IN CORE

	Energy Loss/Fission (MeV)								<u>Leakage</u>
				Steel					
	<u>Fuel</u>	<u>Sodium</u>	<u>BeO</u>	<u>Fuel Clad</u>	<u>BeO Clad & Tightener Sleeve</u>	<u>Channel & Side Rods</u>	<u>Steel Center in B₄C Rods</u>	<u>B₄C</u>	
Fission Product Absorption	172.								
Beta Absorption	9.								
Gamma Absorption	14.8	0.9	0.7	2.2	0.8	3.1	0.2	0.04	
Alpha Absorption								0.13	
Elastic Scattering	1.0	0.2	0.5						
Gamma Leakage									
Neutron Leakage	—	—	—	—	—	—	—	—	
TOTAL	196.8	1.1	1.2	2.2	0.8	3.1	0.2	0.17	

3.2.2 Radial and Axial Core Power Distributions

A two-dimensional contour plot of the power density distribution is given in Figure 3-1. The radial and axial power density distributions are plotted in Figures 3-2 and 3-3. Comparisons of the U-239 and Pu-239 fission distributions between theory and experiment were made on the SEFOR mockup in ZPR-3. The accuracy of these comparisons is discussed in Section 3.2.3.

Removal of a reflector control rod distorts the radial neutron power distribution. The calculated radial power distributions with one rod withdrawn are plotted in Figure 3-4.

The plots in Figures 3-2 and 3-3 were obtained from one-dimensional 18-group DEMON diffusion theory calculations (problem numbers C4C01 and C4A01, respectively). The curves in Figure 3-1 were obtained from a 4-group PDQ diffusion theory calculation in r,z geometry. The curves in Figure 3-4 were obtained from a 2-D, 4 group, PDQ calculation in x,y geometry in which one-half the core was included in the calculation to provide the required symmetry.

FIGURE 3-1. RADIAL DISTRIBUTION OF POWER DENSITY

FIGURE 3-2. AXIAL DISTRIBUTION OF POWER DENSITY

FIGURE 3-3. TWO-DIMENSIONAL CONTOUR PLOT OF POWER DENSITY (RELATIVE TO PEAK VALUE OF 0.295)

FIGURE 3-4. ASYMMETRY IN RADIAL POWER DENSITY DISTRIBUTION CAUSED BY REMOVAL OF ONE REFLECTOR CONTROL ROD

3.2.3 Power Peaking Factors

3.2.3.1 Results

The ratios of peak-to-average power densities (power peaking factors) are listed in Table 3-7. The total error of $\pm 3\%$ (considered equivalent to one standard deviation) is discussed in the next section.

TABLE 3-7

POWER PEAKING FACTORS

	<u>1-Dimensional Calculations</u> (18 energy groups)	<u>2-Dimensional (r,z) Calculations</u> (4 energy groups)	<u>2-Dimensional (x,y) Calculations</u> (4 energy groups)
Radial	1.47	} 1.85	
Axial	1.24		
Local	(1.01)	(1.01)	1.01
Total	1.84 $\pm 3\%$	1.87	

The local peaking factor results from withdrawal of one reflector control rod about one-third down from the completely inserted position at 20 MW. This position provides the maximum of 50¢ excess allowed at 20 MW. For the case shown in Figure 3-4, the local peaking factor was 1.04. However, withdrawal of a complete rod at 20 MW would reduce reactivity by 1.6\$ which is three times the allowable excess; hence, the rod can only be withdrawn one-third of the way at 20 MW with a resulting local peaking factor of only 1.01.

3.2.3.2 Estimated Uncertainties in Power Peaking Factors

The propagated uncertainty in the overall power peaking factor is estimated to be $\pm 3\%$, assuming the uncertainties to be random and uncorrelated. This uncertainty, and all uncertainties described below, are considered to have the significance of one standard deviation. Hence, there is little chance that the true overall peaking factor is more than 6% higher than the values quoted in Table 3-7.

(a). Measurement of Power Distribution

The uncertainties in the axial and radial peaking factors are estimated by comparing the measured fission traverses in ZPR-3 with the calculated ZPR-3 power distribution. The fission traverses were obtained by moving a small fission chamber through a 1/2-inch diameter tube which was inserted into the reactor. The measured and calculated traverses are shown in Reference 2 .

The "experimental" power distribution is obtained by the following technique. The peak-to-average distribution is obtained for the experimental Pu-239 and U-238 traverses. The central U-238/Pu-239 fission ratio, measured in ZPR-3, and the homogenized atomic densities are used to determine the relative number of fission from each isotope at the center of the core. This determines the weighting factor for each traverse at the core center. The "experimental" power distribution is then the composite of the two weighted fission traverses.

The same technique is used to determine the calculated power distribution except the calculated traverses and fission ratios are used.

(b). Axial Power Distribution - Comparison with Experiment

The calculated peak-to-average peaking factor (P/A) agreed within 1% of the "measured" P/A; the calculated value was 1% lower than the measured value. Close agreement in the axial traverse is possible due to the lack of local perturbations influencing the axial traverses. In other words, the axial traverse direction was parallel with the orientation of the fuel plates so that local variations in fuel density do not occur in the axial direction. The close agreement between the measured and calculated axial power traverse, therefore, signifies that the calculational technique was accurate.

The uncertainty in the SEFOR axial P/A was, consequently, estimated to be $\pm 1\%$.

(c). Radial Power Distribution - Comparison with Experiment

The calculated radial P/A agreed within 2% of the measured P/A value; the calculated value was 2% higher than the measured value.

The Pu-239 fission curve shown in reference 2 shows a dip in the measured value of the power near the center of the core. This dip resulted from a reduction in the amount of plutonium near the center of the core transverse to the direction in which the traverse was made. The power distribution is not measured as accurately in the radial direction as in the axial direction since the radial traverse direction is perpendicular to the fuel plate orientation. The dip in the measured power density was ignored in the comparison between measurement and calculation.

Based on comparison with the SEFOR mockup measurement, the uncertainty in the SEFOR radial peak-to-average value was estimated to be $\pm 2\%$.

(d). Sensitivity of the Calculated P/A to Number of Energy Groups

The sensitivity of the P/A power distribution to the number of energy groups was investigated in spherical geometry as part of the analysis of Assembly 48. From 8 to 24 energy groups were used showing differences in the P/A of $< 0.2\%$.

(e). Sensitivity of the Calculated P/A to Plutonium-239 Alpha Value

The sensitivity of the P/A to the Pu-239 alpha value was also investigated during the Assembly 48 analysis. Using different Pu-239 alpha values (ENDF/B, Uttley-James, G.E. recommended values) showed differences in the P/A of $< 0.5\%$.

(f). Sensitivity of the Calculated P/A to Diffusion Theory vs. Transport Theory

The difference in P/A calculated by diffusion theory and S_4 transport theory was investigated in spherical calculations under AEC program PA-10 [Contract AT-(04-3)-189]. These calculations were performed for a 50 cm radius reflected sphere (similar to SEFOR) with a neutron energy spectrum similar to SEFOR. These calculations showed a difference in P/A of $< 0.5\%$.

(g). Sensitivity of P/A Due to Two-Dimensional Synthesis vs. Actual Two-Dimensional Calculation

The results of Table 3-8 show close agreement (to within 1.5%) between the peak-to-average value obtained from 4-group PDQ (actual two-dimensional) and the value synthesized from 18-group axial and radial calculations. Additional experience in the PA-10 program shows differences varying between 0 and $\sim 3\%$ between CRAM (actual 2-D) and BISYN (2-D synthesis). Hence, the uncertainty estimated for the 2-D synthesis peak-to-average value used for SEFOR was $\pm 2\%$.

(h). Local P/A Due to Reflector Control Rod Movement

The local power peaking is calculated from the change in P/A when one reflector rod is partially withdrawn. At 20 MW, only 50¢ of the reflector may be withdrawn. These calculations, 4-group PDQ in x-y geometry, showed the local peaking factor to be $1.01 \pm .005$ ($\pm .5\%$).

(i). Summary of Uncertainties in P/A Values

The uncertainties in the calculated peak-to-average power distribution are summarized in Table 3-8.

TABLE 3-8

UNCERTAINTIES IN CALCULATED PEAK-TO-AVERAGE POWER DISTRIBUTION

<u>Calculation</u>	<u>Uncertainty</u>
Axial	$\pm 1\%$
Radial	$\pm 2\%$
Local	$\pm 0.5\%$
2-D Synthesis vs. Actual 2-D Calculation	$\pm 2\%$
Diffusion vs. Transport Theory	$\pm 0.5\%$
Number of Energy Groups	$\pm 0.2\%$
Pu-239 Alpha Value	$\pm 0.5\%$
Total Calculated Uncertainty (Randomly Propagated)	$\pm 3\%$

3.2.4 Asymmetric Power Density in SEFOR Fuel Rod Due to BeO Rod

A question of concern to the SEFOR core design was whether the BeO rod at the center of each SEFOR fuel channel caused an asymmetric power distribution in the adjacent fuel rods. Were this effect significant, it would cause fuel rod bowing.

Transport theory cell calculations, together with an examination of cross sections in and around the fuel rod, show that the asymmetry due to the BeO rod is insignificant. The power density on the side of the fuel rod adjacent the BeO rod is less than 2% higher than the power density at the opposite side of the rod. This result is in agreement with diffusion-theory cell calculations made early in the SEFOR design.*

Asymmetry in the power distribution was examined at three levels;

- (1) a transport theory (S4 approximation) calculation of the cell was made,
- (2) the scattering cross section distribution around a fuel rod was examined,
- (3) the fission and total mean free paths in the fuel at the peak of the resonances were calculated at 1 keV.

3.2.4.1 Transport Theory Cell Calculation

An 18-group cell calculation of the power distribution across a rod was made using the ANISN transport theory code in the S4 approximation. The calculated ratio of power densities on opposite sides of the fuel rod (peak-to-minimum) was 1.012. A diffusion theory calculation of the same cell gave a peak-to-minimum ratio of 1.007. The peak-to-average ratio calculated with the S4 transport approximation was 1.007.

* G.R. Pflasterer, "SEFOR Experimental Program Planning, Vol. I," GEAP-5092 P. I-60, August, 1966.

The SEFOR cell, together with the mockup used in ANISN, is shown in Figure 3-5.

3.2.4.2 Scattering Cross Section Around the Rod

One reason why the asymmetry in the power distribution in the fuel rod is so small is that the asymmetry in scattering cross section around the fuel due to the BeO rod is not really very large. On the side opposite the BeO are the steel side rods, the steel channel walls, and sodium, as can be observed in Figure 3-6. The macroscopic scattering cross sections in several areas around the rod were calculated for two energy groups--group 13 (0.5 to 1.0 keV) and group 8 (9 to 25 keV). These scattering cross sections are shown in Figure 3-6.

3.2.4.3 Fuel Resonance Cross Sections

There existed the possibility that large resonances might cause asymmetric power production on the side of the BeO rod since the macroscopic scattering cross sections were slightly higher on that side. The effect of these resonances would not be accounted for by the 18-group transport theory calculation since resonances are averaged out over the energy groups. Calculation of the peak resonances cross sections at 1 keV show, however, that the resonances are not large enough to invalidate the results obtained from the cell calculations.

The mean free path for fission in the SEFOR fuel for a neutron at the peak resonance energy is large, relative to the fuel rod diameter. At 1 keV, the calculated fission cross section for Pu-239 at the peak of a Doppler broadened resonance for the J=1 state is 17.6 barns. The value for the J=0 state and values at higher neutron energies are lower. The fission mean free path for the SEFOR Pu-239 concentration and for the 17.6 barn cross section is 14 cm, or 6 times the rod diameter. The total effective mean free path at the resonance

REGION	RADIUS (cm)	VOLUME FRACTION			
		FUEL	SS	Na	BeO
1	1 0097	--	--	--	1 00
2	1 6510	--	0 3256	0 6744	--
3	3 9116	0 6096	0 0947	0 2957	--
4	4 2143	--	0 8461	0 1539	--

FIGURE 3-5. SEFOR CELL AND CALCULATION MODEL

	θ	360
$\theta_1 = 22^\circ$	0.061	
$\theta_2 = 40^\circ$	0.111	
$\theta_3 = 46^\circ$	0.128	
$\theta_4 = 47\frac{1}{2}^\circ$	0.132	
$\theta_5 = 24^\circ$	0.067	
$\theta_6 = 23^\circ$	0.064	

$R_A = 0.4375$ inches
$R_B = 0.6312$
$R_C = 0.875$
$R_D = 1.25$
$R_E = 1.70$

FIGURE 3-6. DISTRIBUTION OF SCATTERING MATERIAL AND MACROSCOPIC SCATTERING CROSS SECTION

peak is ~ 1.3 cm. Hence, no additional spatial peaking of the power would be expected at the edge of a fuel rod due to resonance fission above that appearing in a normal cell calculation, and one would, therefore, expect the multi-group cell calculations to be valid.

The resonance cross sections at 1 keV were calculated as follows:

The non-Doppler broadened total plutonium cross section at the center of a resonance is:

$$\sigma_o = 2.6 \times 10^6 \frac{g\Gamma_n}{E_o\Gamma} \text{ (barns)}$$

where $g = 3/4$ for the $J=1$ state for Pu-239.

For 1 keV ($E_o = 1000$ eV) for Pu-239

$$\Gamma_n^o = 0.334 \times 10^{-3} \text{ (eV)}^{1/2}$$

$$\Gamma_n = 3\Gamma_n^o = 3 \Gamma_n^o \sqrt{E_o} = 0.318 \text{ eV}$$

$$\Gamma_\gamma = 0.0387 \text{ eV}$$

$$\Gamma_n = 0.055 \text{ eV}$$

$$\Gamma = \Gamma_n + \Gamma_\gamma + \Gamma_f = 0.1255 \text{ eV}$$

the value used for Γ_n is three times the average Γ_n since the reaction rate is dependent on $\langle \Gamma_n^2 \rangle$ instead of $\langle \Gamma_n \rangle^2$, and, for the $v=1$ distribution followed by Γ_n , $\langle \Gamma_n^2 \rangle / \langle \Gamma_n \rangle^2 = 3$.

The peak fission cross section is:

$$\sigma_{o,f} = \sigma_o \frac{\Gamma_f}{\Gamma} = (495) \left(\frac{0.055}{0.1255} \right) = 217 \text{ barns}$$

To obtain the Doppler broadened peak cross section, the above value is multiplied by the Doppler line shape $\psi(x)$ at the resonance center ($x=0$), or $\psi(0)$. At $x=0$,

$$\psi(0) = \sqrt{\pi} \frac{\xi}{2} e^{(\xi/2)^2} \operatorname{erfc} \frac{\xi}{2}$$

where: $\xi = \Gamma/2\Delta$

$$\Delta = \sqrt{\frac{4kE_o T}{A}} \quad (\text{Doppler width})$$

for $T = 800^\circ\text{K}$ (i.e. the value at the edge of a fuel rod)

$$k = 0.861 \times 10^{-4} \text{ eV}/^\circ\text{K}$$

$$A = 239$$

$$E_o = 1000 \text{ eV}$$

$$\text{then } \Delta = 1.07 \text{ eV}$$

Hence, $\psi(0) = 0.081$.

Finally, the Doppler broadened peak fission cross section is:

$$\sigma_{o,f} \psi(0) = (217) (.081) = 17.6 \text{ barns}$$

In a fuel rods, $N(UO_2 + PuO_2) = .0228$. For the SEFOR fuel enrichment of 18.7% Pu-239, $N(Pu-239) = 0.0042$. Hence, the fission mean free path at the peak of the resonance, λ_f , is:

$$\lambda_f = \frac{1}{(0.0042)(17.6)} = 14 \text{ cm}$$

The total mean free path at the peak of resonance, λ_t , is:

$$\begin{aligned} \lambda_t &= \frac{1}{N(UO_2 + PuO_2) \sigma_p + N(Pu) \sigma_o \psi(0)} \\ &= \frac{1}{(.0228)(20) + (.0042)(495)(.081)} \\ &= 1.3 \text{ cm} \end{aligned}$$

3.3 NEUTRON FLUX AND ENERGY SPECTRUM

3.3.1 Total Flux

The peak calculated total neutron flux at 20 MW is 6×10^{14} neutrons/cm²-sec. The average neutron flux is 3×10^{14} neutrons/cm²-sec.

These values were obtained from the peak flux in the axial DEMON problem C4A01 (3.9×10^{14}) and using the value 1.47 for the radial peak-to-average and the value 1.24 for the axial peak-to-average.

3.3.2 Flux and Adjoint Spatial Distribution

The radial and axial flux distributions are plotted in Figures 3-7 and 3-9 for three (of 18) energy groups -- groups 2 (1.35-3.7 MeV), group 6 (67-180 keV) and group 12 (1.0-2.0 keV). Radial fluxes were obtained from DEMON case C4C01. Axial cases from C4A01. The values plotted are average fluxes in the particular dimension at 20 MW. Peak values (along the midplane or centerline) can be estimated by multiplying the average values by the appropriate peak-to-average power values -- 1.24 for the radial case and 1.47 for the axial case.

The radial and axial adjoint flux distributions are plotted in Figure 3-8 and 3-10 for groups 2, 6, and 12.

3.3.3 Neutron Flux and Fission Event Energy Spectra

The median fission energy at the core center is 207 keV. The median fission energy averaged over the core is 174 keV. Table 3-9 gives the spectrum of fission events in both 18 groups and 4 energy groups at the core center and averaged over the core. Table 3-10 gives the integrated flux for 18 groups at both the core center and averaged over the core. Figure 3-11 gives a plot of the

FIGURE 3-7. RADIAL FLUX DISTRIBUTIONS

FIGURE 3-8. RADIAL ADJOINT FLUX DISTRIBUTIONS

FIGURE 3-9. AXIAL FLUX DISTRIBUTION

FIGURE 3-10. AXIAL ADJOINT FLUX DISTRIBUTIONS

TABLE 3-9

INTEGRATED FISSION EVENT SPECTRUM
(and Energy Group Structures)

One-Dimension (Radial)					Two-Dimension (r-z)			
<u>Group</u>	<u>E_g</u>	<u>Δu</u>	<u>@ Core Center</u>	<u>Core Average</u>	<u>Group</u>	<u>E_g</u>	<u>@ Core Center</u>	<u>Core Average</u>
1	3.7 MeV	1.0	.0350	.0330				
2	1.35	1.0	.2084	.1968	1	1.35 MeV	.2078	.1946
3	0.825	0.5	.2692	.2546				
4	0.50	0.5	.3589	.3404				
5	0.18	1.0	.5207	.4960	2	0.18 MeV	.5266	.4948
6	67 keV	1.0	.6635	.6334				
7	25	1.0	.7561	.7226				
8	9.1	1.0	.8324	.7971	3	9.1 keV	.8410	.7971
9	5.5	0.5	.8570	.8212				
10	3.3	0.5	.8763	.8402				
11	2.0	0.5	.8901	.8536				
12	1.0	0.7	.9323	.8983				
13	0.50	0.7	.9643	.9347				
14	0.25	0.7	.9878	.9657				
15	55.6 eV	1.5	.9995	.9913				
16	12.4	1.5	1.0000	.9969				
17	0.625	3.0	1.0000	.9998				
18	Thermal	-	1.0000	1.0000	4		1.0000	1.0000

TABLE 3-10

INTEGRATED RELATIVE NEUTRON FLUX SPECTRUM

<u>Group</u>	<u>E_{ℓ}</u>	<u>Δu</u>	<u>@ Core Center</u>	<u>Core Average</u>
1	3.7 MeV	1.0	.0157	.0153
2	1.35	1.0	.1035	.1012
3	0.825	0.5	.1661	.1628
4	0.50	0.5	.2773	.2728
5	0.18	1.0	.5004	.4944
6	67 keV	1.0	.7026	.6957
7	25	1.0	.8336	.8265
8	9.1	1.0	.9211	.9146
9	5.5	0.5	.9437	.9375
10	3.3	0.5	.9590	.9532
11	2.0	0.5	.9668	.9610
12	1.0	0.7	.9860	.9819
13	0.50	0.7	.9958	.9934
14	0.25	0.7	.9992	.9981
15	55.6 eV	1.5	1.0000	.9998
16	12.4	1.5	1.0000	.9999
17	0.625	3.0	1.0000	1.0000
18	Thermal	-	1.0000	1.0000

FIGURE 3-11. $\phi(E)$ AT PEAK POWER (RADially) VERSUS ENERGY

flux per unit energy $[\phi(E)]$ versus energy near the core center for an 18-group radial calculation; the fluxes at energies below 250 eV are somewhat uncertain since they are strongly influenced by the transverse bucklings. Figure 3-12 shows the flux per unit energy in the gap. Figure 3-13 shows both $\phi(u)$ versus u and $\Sigma_t(u)\phi(u)$ versus u . The strong deviation from constant $\Sigma_t(u)\phi(u)$ in a fast reactor can be observed from Figure 3-13. Figure 3-14 gives the adjoint flux, ϕ^* , as a function of energy at the core center.

3.4 NEUTRON BALANCE AND OTHER CORE PARAMETERS

3.4.1 Delayed Neutron Fraction and Prompt Neutron Lifetime

The calculated value of β_{eff} for SEFOR is 0.0032. Delayed neutron parameters for fast fission recommended by Keepin⁽⁴⁾ were used. β_{eff} is dependent both on the relative number of fissions between U-238 and Pu-239 and on the relative effectiveness of delayed versus prompt neutrons. The relative number of fissions was given above in Table 3-4 and these values were calculated using cross section which gave reasonably good agreement with the measured U-238 to Pu-239 fission ratios in the SEFOR mockup in ZPR-3 (see Reference 1). The relative effectiveness of delayed neutrons to prompt neutrons was calculated for each energy group using data in Reference 3 for the energy dependence of the delayed neutrons. The calculated value of β_{eff} divided by β is 0.91, and this result is essentially the same for each delayed neutron group.

FIGURE 3-12. AVERAGE $\phi(E)$ OVER GAP VERSUS ENERGY

FIGURE 3-13 $\phi(u)$ and $\Sigma_t(u)\phi(u)$ verses Lethargy (u)

FIGURE 3-14. ϕ^* AT PEAK POWER POINT (RADIALLY) VERSUS ENERGY

3.4.2 Prompt Neutron Lifetime

The value of ℓ/β_{eff} was measured in the SEFOR mockup at ZPR-3 to be 2.05×10^{-4} sec. Using this value together with the calculated value of β_{eff} gives the value for the prompt neutron lifetime of 0.66 μsec .

The most accurate SEFOR mockup lifetime calculation is reported in Reference 3. Using a 2-dimensional 16-group CRAM calculation and using cross sections almost equivalent to the SEFOR mockup 60-group cross section set, Greeber, et al, obtained $\ell = 0.65 \mu\text{sec}$. Using the SEFOR mockup 60-group cross sections and 1-dimensional radial calculations, a value of $\ell = 0.54 \mu\text{sec}$ was obtained, however, it is known that the 1-dimensional result is sensitive to the transverse bucklings used. Calculated values were obtained from the calculated reactivity effect of a $1/v$ absorber.

3.4.3 Neutron Balance

A neutron balance for the SEFOR core is listed in Table 3-11. This neutron balance was obtained from 18-group, 1-dimensional radial and axial calculations. Small differences which appeared in the radial and axial neutron balances have been averaged to obtain the best estimate for the SEFOR neutron balance. From the neutron balance is seen that the beryllium $n,2n$ reactivity effect for SEFOR is 0.66%.

TABLE 3-11

SEFOR NEUTRON BALANCE

	<u>Core</u>	<u>Gap</u>	<u>Total</u>
<u>Sources</u>			
a) Fission	.9906	.0028	.9934
b) Be(n,2n)	<u>.0063</u>	<u>.0003</u>	<u>.0066</u>
Total	.9969	.0031	1.0000
<u>Removals</u>			
a) Leakage			
Axial	.1246	-.0258	.0988
Radial	<u>.2218</u>	<u>.0186</u>	<u>.2404</u>
Total	.3464	-.0072	.3392
b) Fissions			
U-235	.0034	.0001	.0035
U-238	.0353	.0009	.0362
Pu-240	.0051	--	.0051
Pu-239	<u>.3038</u>	<u>--</u>	<u>.3038</u>
Total	.3476	.0010	.3486
c) Captures			
Be	.0022	.0001	.0023
Oxy	.0016	--	.0016
Na	.0013	.0001	.0014
Cr	.0033	.0003	.0036
Fe	.0164	.0016	.0180
Ni	.0056	.0005	.0061
Mo	.0014	.0002	.0016
U-235	.0013	--	.0013
U-238	.1834	.0062	.1896
Pu-240	.0118	--	.0118
Pu-239	<u>.0749</u>	<u>--</u>	<u>.0749</u>
Total	.3032	.0090	.3122

3.5 DOPPLER COEFFICIENT

3.5.1 Results

For SEFOR, the Doppler coefficient is approximately inversely proportional to the fuel temperature. Hence, the Doppler coefficient defined as $T \frac{dk}{dT}$ is approximately constant. For the Core I SEFOR design the value of $T \frac{dk}{dT}$ is -0.0085. This value assumes the 2-segment core design and assumes a boron carbide content equivalent to 3.5% in reactivity. The contribution to $T \frac{dk}{dT}$ from U-238 was -.0082; the contribution from Pu-230 was -.0003; and the contribution from Pu-239 was assumed to be zero based on the experimental measurements of the plutonium Doppler effect in the SEFOR mockup in ZPR-3.

The reactivity effects due to Doppler for $T \frac{dk}{dT} = -0.0085$ are:

Refueling temperature (350°F) to 20 MW: $\Delta k_D = -3.0\%$

Refueling temperature to zero power, 760°F: $\Delta k_D = -1.1\%$

Zero power, 760°F, to 20 MW: $\Delta k_D = -1.9\%$

The WEDOP computer code⁽⁵⁾ was used to calculate Δk_D as a function of power from 0 to 20 MW, for a Doppler coefficient of $T \frac{dk}{dT} = -0.0085$, and for a constant average sodium temperature of 760°F. The results are shown in Figure 3-15.

Also shown in Figure 3-15 is a least squares fit to the Δk_D versus power curve based on the following model:

$$\Delta k_D = \left(T \frac{dk}{dT} \right) \ln \left(1 + \frac{R_f P}{T_{co}} \right)$$

where $R_f = 65.1$ (OR/MW)

$P =$ Power (MW)

$T_{co} =$ Sodium temperature (or fuel temperature at zero power) (°F)

FIGURE 3-15. DOPPLER REACTIVITY VERSUS POWER

The calculated Doppler effect for the actual temperature distribution obtained from WEDOP is about 10% higher than the value calculated assuming an isothermal temperature distribution using average fuel temperatures.

3.5.2 Dependence of the Doppler Coefficient on Temperature

The use of a constant value for $T \frac{dk}{dT}$ represents a close approximation for SEFOR. The actual calculated temperature dependence of $\frac{dk}{dT}$ was $T^{-0.9}$ based on the data shown in Table 3-12.

TABLE 3-12

DEPENDENCE OF CRITICALITY FACTOR ON TEMPERATURE

<u>U²³⁸ Temperature</u>	<u>Criticality Factor</u>
300°K	1.04804
700°K	1.03983
1400°K	1.03245
3000°K	1.02389
5000°K	1.01804

Listed in Table 3-12 are the criticality factors calculated as a function of U-238 fuel temperature using 18-group spherical calculations in which the U-238 fuel temperature was assumed constant throughout the core and represented the only change between the problem. The $T^{-0.9}$ variation corresponds closely to the ZPR-3 measurements of the Doppler effect between 300°K and 1100°K, although the data is insufficient to establish the temperature variation accurately. As a practical matter the reactivity effect is fairly insensitive to the choice of -1 or -0.9 for the exponent and therefore the constant value for $T \frac{dk}{dT}$ has been assumed for SEFOR.

3.5.3 Influence of Spatial Fuel Temperature Distribution on the Doppler Effect

The spatial dependence of the Doppler reactivity, Δk_D , was calculated by means of the WEDOP computer code. Starting with the isothermal Doppler coefficient, $T \frac{dk}{dT}$, WEDOP calculates the Doppler reactivity from the relation,

$$\Delta k_D = \left(T \frac{dk}{dT} \right) \frac{\int p^2(r,z) \ln \frac{T_2(r,z)}{T_1(r,z)} dV}{\int p^2(r,z) dV} \quad (1)$$

where $\ln T(r,z)$ is the average value of $\ln T(r,z)$ in the fuel rod at (r,z) , and T is in degrees absolute, and $p(r,z)$ is the power density distribution. $T(r,z)$ and the temperature distribution in the fuel rods are calculated in WEDOP from the input power distribution.

Validity of Power Squared Weighting Used in WEDOP

The U-238 Doppler effect may be represented as being proportional to the volume integral of the flux-adjoint product $(\phi\phi^*)$ weighted change in U-238 capture cross section ($\Delta\sigma_c$):

$$\Delta k_D = \frac{C \int \sum_{i=1}^I \phi_i(r,z) \phi_i^*(r,z) \Delta\sigma_{ci}(r,z) dV}{\int \sum_{i=1}^I \phi_i(r,z) \phi_i^*(r,z) dV} \quad (2)$$

where I = number of neutron energy groups.

In one-dimension, Equation (2) can be compared with Equation (1) in the following manner.

If we assume that

$$\Delta\sigma_{ci}(r) = \Delta\sigma_{ci} f(r)$$

then, for a local Doppler coefficient of the form

$$T \frac{dk}{dT} = \text{constant},$$

the function $f(r)$ is

$$f(r) = \ln \frac{T_2(r)}{T_1(r)}$$

where $T_1(r)$ and $T_2(r)$ are the fuel temperatures at r before and after the change in power.

In one dimension, Equation (2) can be written as

$$\Delta k_D = \frac{C \int \sum_{i=1}^I \phi_i(r) \phi_i^*(r) \Delta\sigma_{ci} \ln \frac{T_2(r)}{T_1(r)} dV}{\int \sum_{i=1}^I \phi_i(r) \phi_i^*(r) dV} \quad (3)$$

To solve for C , we define the isothermal Doppler coefficient, $T \frac{dk}{dT}$, as

$$\Delta k_{D, \text{isothermal}} = T \frac{dk}{dT} \ln \frac{\langle T \rangle_2}{\langle T \rangle_1} = \frac{C \int \sum_{i=1}^I \phi_i(r) \phi_i^*(r) \Delta\sigma_{ci} \ln \frac{\langle T \rangle_2}{\langle T \rangle_1} dV}{\int \sum_{i=1}^I \phi_i(r) \phi_i^*(r) dV} \quad (4)$$

Solving Equation (4) for C and substituting into (3) gives

$$\Delta k_D = T \frac{dk}{dT} \frac{\int \sum_{i=1}^I \phi_i(r) \phi_i^*(r) \Delta \sigma_{ci} \ln \frac{T_2(r)}{T_1(r)} dV}{\int \sum_{i=1}^I \phi_i(r) \phi_i^*(r) \Delta \sigma_{ci} dV} \quad (5)$$

Equation (5) can be compared directly with Equation (1) in the radial and axial directions to assess the applicability of the power-squared weighting function. This comparison was made for SEFOR for $T_2(r)/T_1(r)$ corresponding to a change from zero power to 20 MW. The calculated distributions of the power squared and the flux-adjoint product weighting functions are shown in Figures 3-16 and 3-17 for the radial and axial cases, respectively. These figures show that the difference in the shape of the P^2 and $\phi\phi^*$ weighting functions is most pronounced in those regions where the change in neutron spectrum is most pronounced, e.g., near the FRED channel, the axial fuel gap, and the edge of the core. Results of this analysis indicate that the local differences between the two weighting functions are largely self-compensating when volume integration of the local Doppler effect is performed. For the axial calculation the error introduced by P^2 weighting was calculated to be +0.04%. For the radial calculation the corresponding error was +1.7%. The percentage error should remain about the same in a case where the change of power between two equilibrium conditions is less than 20 MW. Therefore, it is expected that use of power-squared (rather than $\phi\phi^*$) weighting of the local Doppler effect will introduce less than 2% error in the determination of the Doppler coefficient.

FIGURE 3-16. SEFOR DOPPLER WEIGHTING FUNCTIONS - RADIAL CASE

FIGURE 3-17. SEFOR DOPPLER WEIGHTING FUNCTIONS-AXIAL CASE

It was reported in Section 3.5.1 that the Doppler reactivity calculated for the actual temperature distribution is ~10% higher than the value using isothermal average temperature changes. It is interesting to observe the source of this 10% difference. If we assume separability of power and fuel temperature distributions such that

$$p(r,z) = P_o p(r) p(z)$$

$$T(r,z) = \langle T \rangle t(r) t(z)$$

then Equation (1) can be written as

$$\begin{aligned} \Delta k_D &= T \frac{dk}{dT} \frac{\iint p^2(r) p^2(z) \left[\ln \frac{\langle T_2 \rangle}{\langle T_1 \rangle} + \ln \frac{t_2(r)}{t_1(r)} + \ln \frac{t_2(z)}{t_1(z)} \right] r dr dz}{\iint p^2(r) p^2(z) r dr dz} \\ &= T \frac{dk}{dT} \left[\ln \frac{\langle T_2 \rangle}{\langle T_1 \rangle} + \frac{\int p^2(r) \ln \frac{t_2(r)}{t_1(r)} r dr}{\int p^2(r) r dr} + \frac{\int p^2(z) \ln \frac{t_2(z)}{t_1(z)} dz}{\int p^2(z) dz} \right] \end{aligned} \quad (6)$$

The first term in Equation (6) is the isothermal Doppler effect. The second and third terms account for the deviation due to the actual radial and axial temperature distributions, and they add 8% and 2% to the first term, respectively.

3.5.4 Variation of Doppler Coefficient with Neutron Energy

The principal contribution to the Doppler coefficient in a fast oxide reactor such as SEFOR comes from neutrons in the energy range between 0.5 keV and 10 keV. The relative contribution to the U-238 Doppler coefficient as a function of energy can be obtained from the relation:

$$\frac{\delta\sigma_{c,i} \phi_i \phi_i^*}{\sum \delta\sigma_{c,i} \phi_i \phi_i^*}$$

where i refers to the energy group, $\delta\sigma_{c,i}$ refers to the difference in U-238 capture cross section between two temperatures, and ϕ_i and ϕ_i^* are the flux and adjoint fluxes.

The values of this ratio as a function of energy group are listed in Table 3-13. In addition the integrated value is listed to show the contribution above each energy. The values refer to the Doppler energy spectrum at the center of the core. This spectrum predominates over most of the core, although softening of the neutron and, hence, the Doppler spectrum does occur near the boundaries.

For completeness, values of $\delta\sigma_{c,i}$ for U-238 from 700°K to 1400°K which were used in the calculation are listed in Table 3-13. Also listed are the 1400°K values used for SEFOR for $\sigma_{c,i}$ (U-238), $\sigma_{f,i}$ (Pu-235), $\sigma_{c,i}$ (Pu-239), and $\sigma_{c,i}$ (Pu-240) since these cross sections influence the Doppler strongly by affecting the flux and adjoint spectra and since the recommended values for these cross sections continually change.

TABLE 3-13

ENERGY DEPENDENCE OF U²³⁸ DOPPLER EFFECT

Group	E _{lower}	$\frac{\delta\sigma_{c,i\phi_i\phi_i}^*}{\sum_i \delta\sigma_{i\phi_i\phi_i}^*}$	Integrated Doppler Spectrum	Cross Sections*				
		$\delta\sigma_c$ (U-238)		σ_c (U-238)	σ_f (Pu-239)	σ_c (Pu-239)	σ_c (Pu-240)	
1 - 4	500 keV	0	0	0				
5	180	0	0	.00006	.15	1.53	.16	.26
6	67	.01	.01	.00054	.24	1.50	.21	.45
7	25	.03	.04	.0028	.37	1.49	.34	.71
8	9.1	.09	.13	.0099	.52	1.83	.80	.97
9	5.5	.06	.19	.0216	.61	2.23	1.22	1.18
10	3.3	.08	.27	.0396	.67	2.56	1.37	1.71
11	2.0	.06	.33	.0625	.71	3.41	1.93	3.08
12	1.0	.24	.57	.0876	.79	3.96	2.33	4.67
13	0.50	.26	.83	.1619	1.14	5.36	3.24	7.28
14	0.25	.11	.94	.1375	.99	9.70	6.09	11.5
15	55.6 eV	.06	1.00	.1854	1.44	15.8	9.99	11.4
16	12.4	0	1.00	.0794	2.09	16.4	14.0	25.9
17	0.625	0	1.00	.1310	3.33	21.6	14.9	65.4
18	Thermal	0	1.00	0				

* Cross Sections correspond to the following material numbers
in Cross Section File Number A18C4, on record at GE-APO.

U-238 ($\delta\sigma_c$) -- 682
 U-238 (σ_c) -- 680
 Pu-239 -- 691
 Pu-240 -- 690

3.5.5 Effect of Boron Carbide Rods on the Doppler Coefficient

The addition of boron carbide rods to SEFOR for reactivity shimming depresses the low energy neutron flux and therefore reduces the Doppler coefficient. An amount of boron carbide equivalent to 1% in reactivity reduces the magnitude of $T \frac{dk}{dT}$ by 0.00025. The $T \frac{dk}{dT}$ value of -0.0085 was calculated for 3.5% Δk worth of B_4C present.

3.5.6 Effect of Loss of Sodium on the Doppler Coefficient

Loss of sodium from SEFOR hardens the neutron spectrum and therefore results in a reduction in the Doppler coefficient. The amount of the reduction was measured in the SEFOR mockup experiments in ZPR-3 by removing sodium from the area around the Doppler samples. The measured reduction in Doppler coefficient due to loss of sodium was 17.5%. Therefore, with loss of sodium the SEFOR Doppler coefficient would be reduced from -0.0085 to -0.0070.

3.5.7 Effect of Self-Shielding on the Doppler Coefficient

Resonance self-shielding for the heavy isotopes was accounted for by calculating average cross sections from resonance data as a function of effective potential scattering cross section, $\sigma_{p,eff}$, per absorber atom. The effect of heterogeneity on $\sigma_{p,eff}$ was accounted for by means of the rational approximation for leakage in an infinite array of rods, in the form derived by Bell:

$$\sigma_{p,eff} = \sigma_{p,1} \left(1 + \frac{\tau}{\Sigma_{p,2}} \right)$$

where

$$\tau = \frac{\frac{S}{4V_1}}{1 + \frac{S}{4V_1} / \Sigma_{p,2}}$$

$$\sigma_{p,1} = \Sigma_{p,1}/N_1$$

Region 1 refers to the fuel rod. Region 2 refers to the annular ring of non-fuel material surrounding the fuel rod.

A comparison was made of the effect of the use of the effective σ_p on the Doppler coefficient and criticality factor, relative to the homogeneous σ_p . Use of $\sigma_{p,eff}$ increased both the Doppler coefficient and k , as shown in Table 3-14 below.

TABLE 3-14

COMPARISON OF HETEROGENEOUS AND HOMOGENEOUS σ_p

<u>Model</u>	Doppler <u>$T \frac{dk}{dT}$</u>	Criticality Factor <u>k</u>
Heterogeneous	-0.0088	1.044
Homogeneous	-0.0081	1.034

The MORT code (since replaced by CROF) was used for the actual calculation of $\sigma_{p,eff}$ for each isotope for each energy group in the 60-group structure.

3.5.8 Analytical Method for Calculation of $T \frac{dk}{dT}$

The Doppler coefficient $T \frac{dk}{dT}$ was calculated assuming an isothermal fuel temperature change from 700°K to 1400°K. Spherical geometry was used for the calculation in order to avoid uncertainties due to the use of transverse bucklings. Temperature dependent cross sections for U-238 and Pu-240 were obtained from the RAPTURE computer code⁽⁶⁾ using the U-238 resolved resonance parameters for Garg, et al,⁽⁷⁾ and other parameters as reported in Reference 8. As described in Section 3.5.7 above, heterogeneity was accounted for through the calculation of $\sigma_{p,eff}$. Overlap corrections which may increase the Doppler by ~5% was not included in this calculation.

The techniques used here together with the cross sections used were also used to compare the U-238 Doppler effect with the results measured in the SEFOR mockup with ZPR-3. Close agreement was obtained between the calculation and experiment as reported in Reference 1.

The Pu-239 Doppler coefficient was assumed to be zero based on the ZPR-3 measurements. At ZPR-3 the Doppler effect was measured for both pure $U^{238}O_2$ samples and for 12-1/2% $Pu^{239}O_2$, 87-1/2% $U^{238}O_2$ samples. The Doppler reactivities per kg U-238 for the two samples were identical when it was assumed that the plutonium contribution was zero.

3.6 EXPANSION COEFFICIENTS

The analysis of the SEFOR Doppler experiments requires that non-Doppler temperature dependent reactivity effects (i.e. expansion effects) be separated out of the total measured reactivity effects to obtain the Doppler reactivity. These reactivity effects also describe the reflector control requirements between shutdown and full power. In some cases the expansion reactivity effects can be measured. Other coefficients, however, cannot be measured or can be measured only in combination with others. The expansion reactivity effects and coefficients for the final SEFOR Core I design are described in this section.

3.6.1 Power and Temperature Expansion Coefficients

The expansion reactivity effects were calculated between 3 different reactor conditions: (1) isothermal 350°F, (2) isothermal 700°F, and (3) 20 MW. The total expansion reactivity effect of changing from 350°F to 700°F is -1.63\$; the effect of changing from 350°F to 20 MW is -2.0\$. Table 3-15 lists the various calculated expansion coefficients as a function of change in reactor power and as a function of the change in material temperature for a sodium inlet temperature of 700°F. The coefficients listed represent average values from zero power and an isothermal temperature of 700°F to temperature conditions at 20 MW. The fuel axial expansion temperature coefficient is based on the change in average fuel temperature (instead of shoulder temperature) so that it can be more easily compared with the Doppler reactivity effect. Also listed in Table 3-15 are the reactivity effects from zero power at 350°F to 20 MW. These numbers are comparable to the calculated Doppler effect from zero power at 350°F to 20 MW of -3.0\$ reported in Section 3.5.1. Each of the coefficients is defined below.

TABLE 3-15

EXPANSION COEFFICIENTS

	$\frac{\delta k/k}{\Delta P} \left(\frac{\text{¢}}{\text{MW}} \right)$	$\frac{\delta k/k}{\Delta T_x} \left(\frac{\text{¢}}{^\circ\text{F}} \right)$	$\frac{\delta k}{k} \text{ 350}^\circ\text{F} \rightarrow 20 \text{ MW (¢)}$
Fuel Axial Expansion	-0.395¢/MW	-0.0058¢/°F	-10.4¢
Fuel Clad Axial Expansion	-0.390¢/MW	-0.078¢/°F	-32.6¢
BeO Axial Expansion	-0.018¢/MW	-0.006¢/°F	- 1.9¢
Structure Axial Expansion	-0.013¢/MW	-0.004¢/°F	- 1.7¢
Structure Radial Expansion	-0.715¢/MW	-0.229¢/°F	-94.3¢
Sodium Density in Core	-0.339¢/MW	-0.121¢/°F	-49.1¢
Sodium Density Out of Core	---	-0.029¢/°F	-10.1¢
TOTAL	1.87 ¢/MW		-200.1¢
Isothermal Total Sodium		-0.47¢/°F	
Temperature Coefficient			
(Expansion Effects Only)			

All expansion coefficients were assumed to be vary linearly with respect to the dominating variable, i.e. change in radius, height, etc. These expansion coefficients, listed in Table 3-16, were calculated from a series of one-dimensional DEMON slab and cylindrical and two-dimensional PDQ problems. The temperature dependent coefficients were determined by calculating the change in height, radius, mass, etc., for the various changes in reactor condition and applying the calculated linear coefficients. The temperature dependent reactivity effects were calculated in Table 3-17.

Details of the methods and constants used for calculating atom densities and core dimensions as a function of temperature are given in Appendix A.

TABLE 3-16

LINEAR EXPANSION AND MASS COEFFICIENTS

<u>Coefficient</u>	<u>Value^(b)</u>
Fuel Axial Expansion $\frac{\Delta k/k}{\% \Delta h_f/h_f}$	-19.27¢
Fuel Clad Axial Expansion $\frac{\Delta k/k}{\% \Delta h_{fc}/h_{fc}}$	-73.31¢
BeO Axial Expansion $\frac{\Delta k/k}{\% \Delta h_{BeO}/h_{BeO}}, \frac{-\Delta k/k}{\% \Delta \rho_{BeO}/\rho_{BeO}}$	- 9.15¢
Structure Axial Expansion ^(a) $\frac{\Delta k/k}{\% \Delta h_s/h_s}, \frac{-\Delta k/k}{\% \Delta \rho_s/\rho_s}$	- 4.00¢
Structure Radial Expansion $\frac{\Delta k/k}{\% \Delta R/R}$	-223¢
Sodium Density in Core $\frac{\Delta k/k}{\% \Delta \rho_{Na}/\rho_{Na}}$	7.25¢
Sodium Density Out of Core $\frac{\Delta k/k}{\% \Delta \rho_{Na}/\rho_{Na}}$	3.24¢

(a). The structure volume fraction is 0.135 and includes channels, side rods, tightener sleeve, and BeO clad. The volume fraction of the fuel clad is 0.081.

(b). β_{eff} is 0.0032.

TABLE 3-17

RESULTS OF EXPANSION REACTIVITY CALCULATIONS

<u>Effect</u>	<u>Change in Condition</u>	<u>% $\Delta h/h$</u>	<u>Value</u>
Fuel _{axial}	350°F→700°F	0.13%	- 2.5¢
	700°F→20 MW	0.41%	- 7.9¢
	350°F→20 MW	0.54%	-10.4%
Fuel clad _{axial}	350°F→700°F	0.34%	-24.9¢
	700°F→800°F	0.11%	- 7.8¢
	350°F→800°F	0.45%	-32.6¢
BeO _{axial}	350°F→700°F	0.17%	- 1.6¢
	700°F→760°F	0.04%	- 0.4¢
	350°F→760°F	0.21%	- 1.9¢
Structure _{axial}	350°F→700°F	0.36%	- 1.4¢
	700°F→760°F	0.06%	- 0.3¢
	350°F→760°F	0.42%	- 1.7¢
Structure _{radial}	350°F→700°F	0.36%	-80.1¢
	700°F→760°F	0.06%	-14.3¢
	350°F→760°F	0.42%	-94.3¢
<hr/>			
		<u>% $\Delta \rho / \rho$</u>	
Sodium _{core}	350°F→700°F	-5.843	-42.3¢
	700°F→760°F	-0.936	- 6.8¢
	350°F→760°F	-6.776	-49.1¢
		<u>$\frac{\Delta(\rho_{Na}^{V_{Na}})_{oc}}{(\rho_{Na}^{V_{Na}})_{oc}}$</u>	
Sodium _{out of core}	350°F→700°F	-3.11%	-10.1¢

3.6.3 FUEL AXIAL EXPANSION EFFECTS

3.6.3.1 Fuel and Clad Expansion Reactivities vs. Power and Inlet Sodium Temperature

The fuel and fuel clad reactivity effects were calculated as a function of power for various sodium inlet temperatures. Figures 3-18 and 3-19 show the fuel axial expansion effect vs. power and inlet sodium temperature, respectively. Figures 3-20 and 3-21 show the fuel clad axial expansion effect versus power and inlet sodium, respectively. The ranges indicated by the curves do not necessarily correspond to the physical limitations of the system. Inlet sodium temperatures from 400°F to ~800°F were considered at zero power and inlet sodium temperatures from 500°F to 800°F were considered with the power ranging from 0 to 20 MW.

The fuel and fuel clad reactivity effects were again assumed linear with respect to the change in height.

The same basic data as listed in Table 3-17 was assumed. The average clad temperature was assumed to increase linearly with power to $T_{Na,in} + 100^{\circ}\text{F}$ @ 20 MW. The average sodium temperature was assumed to increase linearly with power to $T_{Na,in} + 60^{\circ}\text{F}$ @ 20 MW. An average 0.040 inch shoulder temperature at 20 MW was defined as 1357°F for an average sodium temperature of 760°F. This temperature gives the correct expansion of the average rod from 70°F→20 MW. The difference between the average shoulder temperature ($\bar{T}_s$) and the average sodium temperature ($\bar{T}_{Na}$) versus power is shown in Figure -22. These temperatures together with the linear expansion data listed in Table 3-17, yield the change in fuel height as a function of power and inlet sodium temperature.

FIGURE 3-18. AXIAL FUEL EXPANSION REACTIVITY EFFECT VERSUS POWER

FIGURE 3-19. AXIAL FUEL EXPANSION REACTIVITY EFFECT VERSUS INLET SODIUM TEMPERATURE

FIGURE 3-20. AXIAL FUEL CLADDING EXPANSION REACTIVITY EFFECT VERSUS POWER

FIGURE 3-21. AXIAL FUEL CLADDING EXPANSION REACTIVITY EFFECT VERSUS INLET SODIUM TEMPERATURE

FIGURE 3-22. DIFFERENCE BETWEEN $\bar{T}_s$ AND $\bar{T}_{Na}$

3.6.3.2 Contribution to Error in Doppler Coefficient from Axial Fuel Expansion

The contribution of the error in the measurement of the Doppler coefficient in SEFOR due to axial fuel expansion were estimated for 1, 2, and 3-segment fuel designs. This error analysis was one of the contributing factors which led to the selection of the 2-segment design for SEFOR.

The errors were estimated for both dished pellets and flat-ended pellets at high temperature for both a slow power change (corresponding to static and slow oscillator tests) and a fast transient (corresponding to fast oscillator and excursion tests). By fast transient is meant a change for which the fuel temperature feedback is introduced rapidly relative to the ~30 sec fuel time constant.

The techniques used to reduce the error due to axial fuel expansion are segmenting the fuel and dishing the pellets. Both techniques improve the accuracy in tests involving slow power changes. Only segmenting the fuel helps during transients. Segmenting the fuel introduces an important new uncertainty--that due to axial expansion of the clad--during fast transients.* However, this uncertainty is less than that due to axial fuel expansion for unsegmented fuel so that segmenting is still useful for transient tests.

(a). Results and Influence on Selection of 2-Segment Design

The results are given in Table 3-18. Values for the fast transients are essentially the same for both dished and flat-ended pellets. The errors are intended to have the significance of standard deviations.

* An axial clad expansion effect is present in slow power changes, but it is small and predictable and, hence, adds little uncertainty.

TABLE 3-18

CONTRIBUTION TO ERROR IN DOPPLER
COEFFICIENT FROM AXIAL FUEL EXPANSION

	<u>1-Segment</u>	<u>2-Segment</u>	<u>3-Segment</u>
Slow Power Change:			
Dished Pellets	±8%	±3%	±2%
Flat-Ended Pellets	±40%	±17%	±13%
Fast Transients (Dished & Flat-Ended Pellets)			
With clad expansion error	±18%	±10%	±10%
Without clad expansion error	±18%	±9%	±6%

Table 3-18 shows the large reduction in error in going from the 1-segment to the 2-segment design. Little additional advantage is obtained from 2 to 3 segments. The table also shows the large advantage gained in the slow power changes by the use of dished pellets.

The errors in Table 3-18 could rise, particularly for Core II, for the following reasons. For a lower Doppler effect than that calculated, the corresponding errors would increase in proportion to the decrease in the Doppler effect. The values above assumed Doppler coefficients ($T \frac{dk}{dT}$) of -0.0090, -0.0085, and -0.0080 for 1, 2, and 3-segment fuel designs. The Core II Doppler is expected to be about 0.002 less than that for Core I due to the harder Core II neutron spectrum which will be obtained by removal of the BeO in the tightener rods.

(b). Calculation of Reactivity Effects

The errors in Table 3-18 for slow power changes were obtained from calculated axial expansion and Doppler reactivity effects for a steady-state power change from 15 to 20 MW. These reactivity effects are listed for dished and flat-ended pellets in Table 3-19. These high-power levels were chosen in order to study the errors at high temperature where the ratio of the expansion effect to the Doppler effect (and, hence, the error contributed by the expansion effect) is a maximum.

In order to compare errors for a fast transient with those for a slow power change, it is necessary to define a transient which has the same Doppler feedback and, hence, the same average fuel temperature rise. The resulting axial expansion effects for this transient are also listed in Table 3-19. The values for dished and flat-ended pellets were assumed to be the same although there is actually a small difference due to the difference in the linear expansion coefficients for the fuel temperatures at the shoulder and at the center.

TABLE 3-19

REACTIVITY EFFECTS

	<u>1-Segment</u>	<u>2-Segment</u>	<u>3-Segment</u>
Axial Fuel Expansion			
Slow Power Change:			
Dished Pellets	-9.1¢	-2.6¢	-1.0¢
Flat-Ended Pellets	-46.7¢	-13.3¢	-5.0¢
Fast Transient:			
Dished & Flat-Ended Pellets	-20.8¢	-5.9¢	-2.2¢
Axial Clad Expansion in Transient:	0	-1.9¢	-3.0¢
Doppler Effect:	-42¢	-40¢	-38¢

Approximate steady state fuel-rod temperature distribution for the average power density in the core are plotted for 15 and 20 MW in Figure 3-23. These curves illustrate the procedure and are sufficiently accurate for the present estimate of errors in reactivity. The average temperatures for the two curves are 1570°F at 15 MW and 1904°F at 20 MW, which gives a change in average fuel temperature of 334°F. The fuel temperature at the inside of a 40-mil shoulder for dished pellet fuel (the temperature which governs axial expansion for this case) changes from 1110°F to 1260°F between 15 and 20 MW. The fuel temperature at the center (which is assumed for the present analysis to control axial expansion for flat-ended pellets) changes from 2264°F to 2880°F.

In the fast transient the feedback enters before the radial temperature distribution in the rod changes shape. For the present analysis it was assumed that the temperature distribution at the start of the fast transient was the steady state distribution at 15 MW, as shown in Figure 3-23. The axial expansion effect was calculated for the time when the entire temperature curve has risen 334°F corresponding to the average fuel temperature rise between 15 and 20 MW. This uniform rise of 334°F is shown as the dashed curve in Figure 3-23.

The axial expansion reactivity effects were obtained from Figure 3-18.

The increase in clad temperature during the first 0.2 seconds of a large transient is ~8% of the change in average fuel temperature, based on FORE calculations. The axial clad expansion coefficients are 0.07¢/°F and 0.11¢/°F for 2 and 3-segment fuel, respectively; this coefficient is negligible for 1-segment fuel. Use of these coefficients together with the 334°F average fuel temperature rise assumed for this analysis gives the axial clad reactivities shown in Table 3-19.

FIGURE 3-23. RADIAL TEMPERATURE DISTRIBUTION IN
AVERAGE FUEL ROD
(ROD RADIUS = 0.445 Inches)

The Doppler reactivity values listed in Table 3-19 were calculated from the relation

$$\Delta k_D = \left(T \frac{dk}{dT}\right) \ln \frac{\bar{T}_{f2}}{\bar{T}_{f1}}$$

where $\bar{T}_{f1}$ and $\bar{T}_{f2}$ are $(1570 + 460)^\circ\text{R}$ and $(1904 + 460)^\circ\text{R}$ and $T \frac{dk}{dT}$ is -0.0090 , -0.0085 , and -0.0080 for 1, 2, and 3-segment fuel, respectively.

(c). Calculation of Errors

In SEFOR, the calculated value of the axial fuel expansion effect (Δk_e) will be subtracted from the measured (net) fuel temperature effect (Δk_n) to obtain the Doppler effect (Δk_D), i.e.

$$\Delta k_D = \Delta k_n - \Delta k_e$$

The error (σ_D) in the Doppler reactivity, in terms of the errors (σ_n and σ_e) in Δk_n and Δk_e , is

$$\left(\frac{\sigma_D}{\Delta k_D}\right)^2 = \left(\frac{\sigma_n}{\Delta k_D}\right)^2 + \left(\frac{\sigma_e}{\Delta k_D}\right)^2$$

The errors reported in Table 3-18 are values of $\sigma_e/\Delta k_D$

The error in σ_e is composed of three contributions--a reactivity calculational uncertainty [σ_{er}], an error due to uncertainty in fuel movement [σ_{ef}], and an error due to uncertainty in clad expansion [σ_{ec}]. Hence,

$$\sigma_e^2 = [\sigma_{er}]^2 + [\sigma_{ef}]^2 + [\sigma_{ec}]^2$$

The error in fuel movement is estimated to be $\pm 30\%$ * for dished pellets, and it is assumed here that this error holds throughout the 3-year experimental program.** It was also assumed for the present analysis that the $\pm 30\%$ error applies to flat-ended pellets although it is likely that the uncertainty for flat-ended pellets will be greater than for dished pellets.

The error in the reactivity calculation is the error in the calculation if the change in fuel position were known exactly. This error is assumed to be $\pm 20\%$ for 1-segment fuel. This error is based on comparison with the results of the ZPR-3 axial expansion tests, where differences between theory and experiment were $\sim 20\%$.

For segmented fuel, the axial expansion effect is composed of two major components--a negative effect due to expansion of the fuel and a positive effect due to contraction of the gaps. Measurements were made of each component independently in ZPR-3, and the 2 and 3-segment measurements at ZPR-3 show that the small net expansion effect appears to be a compensation of these two large components. Since the components compensate one another, a large part of the error in each component will be compensated and, hence, can be considered to be correlated (i.e. cancel when propagated). For this error analysis the error in each component was assumed to be $\pm 20\%$. It was further assumed that one-third

* R.A. Meyer, GE-APO, personal communication, May, 1966.

** Meyer has allowed a $\pm 15\%$ uncertainty due to changes in fuel expansion with reactor operation in arriving at his recommended $\pm 30\%$ error. In reality, reproducibility of axial fuel movement throughout the SEFOR experimental program will remain an unknown until the program is carried out. Reproducibility will be checked periodically by repeating certain measurements. This unknown, which could introduce errors later in the program larger than the $\pm 30\%$ value assumed, is an added incentive for keeping the axial fuel expansion effect low by the use of segmented fuel.

of the compensated part of each component was uncorrelated. Further details of the calculational errors are given in Section c-1 below.

The uncertainty in the effect of axial fuel clad expansion is important only in the fast transients. For slow power changes the change in clad temperature will be small and predictable. There is still consideration question about how to treat the clad expansion effect during fast transients. For the present, analysis, clad expansion results were taken from FORE⁽⁹⁾ calculations although there is some question whether the FORE treatment is adequate. There is further difficulty in knowing accurately how much the clad were known. Therefore, for the present analysis, an uncertainty of $\pm 100\%$ has been placed on the clad expansion effect.

These errors are translated into numbers for a slow power change from 15 MW to 20 MW and an average fuel temperature rise of 334°F in a fast transient in Table 3-20.

(c-1). Details of Calculation of σ_e (Reactivity Calculation)

For the 1-segment design, σ_e (reactivity calculation) is simply 20% of Δk_e .

For the segmented fuel cases, the Δk_e was assumed to be the sum of two compensated components plus some small remaining uncompensated contribution. It was assumed that the errors in both the uncompensated contribution and in the uncorrelated part (assumed to be 1/3) of the compensated components were $\pm 10\%$. As an example consider the 2-segment, dished pellet, slow power change case. The value of Δk_e is -2.6% . The component of Δk_e due to the change in leakage at the fuel is assumed to be -9.1% , the value for 1-segment fuel. The component contributed by the change in leakage at the gap is assumed to be $+6.5\%$ since the sum of the two components must be -2.6% .

TABLE 3-20

ERROR CALCULATIONS

	<u>1-Segment</u>	<u>2-Segment</u>	<u>3-Segment</u>
Slow Power Change, Dished Pellets:			
Δk_e	- 9.1¢	- 2.6¢	- 1.0¢
σ_e (reactivity calculation)	$\pm 1.8¢$	$\pm 1.0¢$	$\pm 0.8¢$
σ_e (fuel movement)	$\pm 2.7¢$	$\pm 0.8¢$	$\pm 0.3¢$
σ_e	$\pm 3.4¢$	$\pm 1.3¢$	$\pm 0.9¢$
Slow Power Change, Flat-Ended Pellets:			
Δk_e	-46.7¢	-13.3¢	- 5.0¢
σ_e (reactivity calculation)	$\pm 9.3¢$	$\pm 5.4¢$	$\pm 4.8¢$
σ_e (fuel movement)	$\pm 14.0¢$	$\pm 4.0¢$	$\pm 1.5¢$
σ_e	$\pm 16.8¢$	$\pm 6.7¢$	$\pm 5.0¢$
Fast Transient, Dished or Flat-Ended:			
Δk_e	-20.8¢	- 5.9¢	- 2.2¢
σ_e (reactivity calculation)	$\pm 4.2¢$	$\pm 3.0¢$	$\pm 2.0¢$
σ_e (fuel movement)	$\pm 6.2¢$	$\pm 1.8¢$	$\pm 0.7¢$
σ_e (clad expansion)	0	$\pm 1.9¢$	$\pm 3.0¢$
σ_e --with clad expansion error	$\pm 7.5¢$	$\pm 4.0¢$	$\pm 3.7¢$
σ_e --without clad expansion error	$\pm 7.5¢$	$\pm 3.5¢$	$\pm 2.1¢$
Doppler:	-42¢	-40¢	-38¢
Percentage Error:			
Slow change, dished pellets	$\pm 8\%$	$\pm 3\%$	$\pm 2\%$
Slow change, flat-ended pellets	$\pm 40\%$	$\pm 17\%$	$\pm 13\%$
Fast transient, with clad expansion error	$\pm 18\%$	$\pm 10\%$	$\pm 10\%$
Fast transient, without clad expansion error	$\pm 18\%$	$\pm 9\%$	$\pm 6\%$

Since 1/3 of the compensated error is assumed to be uncorrelated, the contribution to the error in the gap leakage component is

$$\sigma(\text{gap leakage}) = +6.5\text{¢} \times 20\% \times 1/3 = \pm .44\text{¢}$$

The contribution to the error in the fuel leakage contribution is

$$\begin{aligned}\sigma(\text{fuel leakage}) &= \Delta k (\text{compensated part}) \times 20\% \times 1/3 \\ &\quad + \Delta k (\text{uncompensated part}) \times 20 \\ &= (-6.5\text{¢} \times 20\% \times 1/3) + (-2.6\text{¢} \times 20\%) \\ &= \pm .96\text{¢}\end{aligned}$$

Finally the error, σ_e (reactivity calculation), is the propagation of the above two values; or

$$\sigma_e (\text{reactivity calculation}) = \sqrt{0.44^2 + 0.96^2} = \pm 1.0\text{¢}$$

3.6.3.3 Additional Analysis of Axial Fuel Expansion

Several characteristics of axial fuel expansion were demonstrated by means of calculation and some were tested in the ZPR-3 experiments. These characteristics include:

- (1). The axial expansion effect for segmented fuel consists of two principal opposing reactivity effects--a negative effect due to increased radial leakage at the fuel segments on expansion (due to the decrease in fuel density) and a positive effect due to decreased radial leakage at the gap

(due to the decrease in gap width). The ZPR-3 experiments indicated a 20% overestimate in the calculation of the negative effect and a 35% overestimate in the calculation of the positive effect. However, the errors tended to compensate for the two-segment case to give agreement between calculation and experiment for the net reactivity.

- (2). The optimum location for the gap in the two-segment design is 10 cm above the fuel midplane.
- (3). The reactivity effect as a function of expansion is nearly linear over the range of interest.
- (4). The expansion reactivities of the various segments and gap are superposable.

The contributions to the net expansion reactivity, showing the dominant role of radial leakage at the fuel and gap, are shown for typical 1-, 2-, and 3-segment calculations in Table 3-21. The results showing the optimum location of the gap and the linearity and the superposition of the expansion reactivity are given in Table 3-22.

TABLE 3-21

CONTRIBUTIONS TO AXIAL FUEL EXPANSION REACTIVITY

Number of Segments	1	2	3
Total Fuel Rod Expansion, Δh (in)	0.54	0.54	0.54
$\Delta k/k$ (Net Calculated Value)	-137¢	-21¢	0 ¢
Contributions to $\Delta k/k$ from Neutron Balance:			
Radial Leakage at Fuel	-122¢	-126¢	-127¢
Radial Leakage at Gap		+102¢	+118¢
Core Absorption	- 26¢	- 22¢	- 20¢
Gap Absorption		+ 7¢	+ 8¢
Axial Leakage	<u>+ 10¢</u>	<u>+ 20¢</u>	<u>+ 17¢</u>
TOTAL	-138¢	- 19¢	- 4¢

TABLE 3-22

CALCULATIONS SHOWING (A) OPTIMUM GAP LOCATION,
(B) LINEARITY, AND (C) SUPERPOSITION OF AXIAL EXPANSION REACTIVITY

A. Optimum Gap Location

<u>Number of Segments</u>	<u>Position of Gap Above Midplane</u>	<u>$\Delta k/k$</u>
2	0	-30¢
	10 cm	-21¢
	20 cm	-42¢

B. Linearity

<u>Number of Segments</u>	<u>Fuel Rod Expansion</u>	<u>$\Delta k/k$</u>
1	0.27 in.	- 57¢
	0.54 in.	-137¢
2	0.27 in.	- 9¢
	0.54 in.	- 21¢
	0.98 in.	- 45¢

C. Superposition

<u>Number of Segments</u>	<u>Portion of Rod Expanded</u>	<u>$\Delta k/k$</u>
2	all	-21¢
	segment above gap	-34¢
	segment below gap	+14¢
3	all	- 0¢
	top segment	-27¢
	middle segment	- 1¢
	bottom segment	+29¢

3.7 SODIUM LOSS EFFECTS

3.7.1 Results

The loss of sodium from the SEFOR reactor results in a large negative reactivity effect. However, loss only from the center portion produces a very slightly positive reactivity effect. Several factors contribute to the total sodium reactivity. On loss of sodium, the spectrum is hardened which results in a positive reactivity effect. Simultaneously, the leakage increases and this gives a negative reactivity. A smaller effect is a positive contribution due to the decrease in sodium capture. Since SEFOR is a small reactor with high leakage, the contribution due to leakage becomes dominant after sodium is lost from a relatively small portion of the core. Hence, the maximum positive reactivity from sodium loss is a small value for SEFOR.

The maximum positive reactivity due to sodium loss was measured in ZPR-3 to be 6¢ for the mockup with no boron carbide present. The presence of boron carbide in SEFOR tends to amplify the hardening of the spectrum contribution and therefore increases slightly the maximum positive effect due to sodium loss. Normalizing the calculated results with the ZPR-3 results, the maximum positive reactivity due to sodium loss from the SEFOR reference design is +17¢. This value assumes an amount of boron carbide present in SEFOR worth 3.5% in reactivity. Uncertainties are present in the extrapolation from the ZPR-3 results to SEFOR which are described in Section 3.7.3. Including the positive uncertainty band, it was concluded that the maximum positive reactivity for the reference design (with 3.5% $\Delta k/k$ B_4C) is $\leq 30\text{¢}$.

It may be necessary to load as much as 7% $\Delta k/k$ worth of B_4C in SEFOR. In that case, the maximum sodium loss value would be increased by an addition 11¢.

The calculated maximum positive reactivity due to sodium loss with no boron carbide present was +3¢. This value is quite close to the experimental value of 6¢ observed in ZPR-3, especially when it is considered that the net value for the sodium loss effect is the sum of two large opposing contributions as shown in the next section.

The calculated value for the loss of sodium from the entire SEFOR core is -4\$. Loss of sodium from the core and vessel results in a reactivity change of -10\$.

All calculations of the sodium loss reactivity effect were made using spherical geometry in order to avoid the use of transverse bucklings which would have led to uncertainties in the spectrum effect. Self-shielding of the cross sections was calculated separately for cases with and without sodium. For calculations in which sodium was voided from part of the reactor, sodium-out cross sections were used in the voided part and sodium-in cross sections were used where sodium was present.

3.7.2 Radial Dependence of the Sodium Loss Reactivity and Contributions to the Sodium Loss Effect

The net calculated reactivities, together with the calculated contributions from spectral hardening, from leakage, and from reduced sodium capture, are given in Table 3-23 as a function of radius voided. Values are shown for both the case without boron and for the case with boron carbide in the core worth 6% in reactivity.

TABLE 3-23

SODIUM LOSS REACTIVITY

<u>Boron Worth</u>	<u>Voided Zone r/R_o</u>	<u>Net (\$)</u>	<u>Leakage Contribution (\$)</u>	<u>Spectrum Contribution (\$)</u>	<u>Na Capture Contribution (\$)</u>
0% Δk	0.4	+ 0.03	- 0.43	+0.42	+0.05
	0.5	- 0.05	- 0.92	+0.78	+0.09
	0.6	- 0.28	- 1.70	+1.27	+0.16
	0.7	- 0.76	- 2.86	+1.85	+0.23
	1.0	- 4.4	- 8.66	+3.76	+0.55
	Vessel	-10.1	-15.63	+4.68	+0.55
6% Δk	0.4	+ 0.18	- 0.35	+0.47	+0.05
	0.5	+ 0.22	- 0.77	+0.88	+0.09
	0.6	+ 0.13	- 1.47	+1.44	+0.15
	0.7	- 0.18	- 2.53	+2.12	+0.21
	1.0	- 3.3	- 8.05	+4.18	+0.50
	Vessel	- 8.8	-14.72	+5.22	+0.50

The values for no B_4C are plotted in Figure 3-24 together with the experimental results obtained for the SEFOR Mockup in ZPR-3. The maximum positive effect of 6¢ observed in the SEFOR mockup in ZPR-3 occurred when sodium was voided from the inner 10% of the core, i.e. for r/R_o of Table 3-23 equal to about 0.5. It is observed from Table 3-23 that this value represents the difference between a leakage contribution of about -90¢ and positive contributions of about 80¢ from spectral hardening and 10¢ from reduction in sodium capture.

FIGURE 3-24. ANALYSIS OF THE SEFOR MOCKUP CRITICAL EXPERIMENTS ON ZPR-3

The relative contributions to the sodium loss reactivity given in Table 3-23 were calculated from the relation:

$$\frac{\delta k}{k} \sim \frac{\delta(v\Sigma_f)}{v\Sigma_f} - \frac{\delta\Sigma_a(\text{non-sodium})}{\Sigma_a} - \frac{\delta\Sigma_a(\text{sodium})}{\Sigma_a} + \frac{\delta P_{NL}}{P_{NL}}$$

where P_{NL} = Non-leakage probability.

The totals calculated from the above relation were normalized to the $\delta k/k$ obtained from DEMON diffusion calculations, although the total $\delta k/k$ obtained from the above relation were in fairly close agreement with the DEMON results prior to normalization. The first two terms make up the spectrum contribution in Table 3-23, with the fission source term being very small (<5%) relative to the absorption term.

3.7.3 Sodium Loss from the Gap

Since the 2-segment design was selected for SEFOR, it was useful to establish experimentally that the loss of sodium from the gap would not cause an additional large positive reactivity. This was done in the SEFOR mockup experiments at ZPR-3 by adding sodium to the gaps of 63 central drawers (which are one-third of the drawers in that half of the mockup). The resulting reactivity was positive, +1.1¢/kg of sodium added. Hence, loss of sodium from the gap would be negative.

3.7.4 Uncertainty in Extrapolation of SEFOR Mockup Measurements of Sodium Loss Reactivity to SEFOR

The uncertainty in the experimental results reported in the ZPR-3 measurements of sodium loss for the SEFOR mockup were $\leq 0.8\%$. The parameters of importance to the sodium loss reactivity--neutron spectrum and leakage--were mocked up quite accurately in the SEFOR mockup. Therefore, it is estimated that the sodium loss effect in SEFOR for a loading without B_4C and of the same composition as the SEFOR mockup would be $\pm 2\%$.

When extrapolating the maximum sodium loss reactivity measured in ZPR-3 to SEFOR, several additional uncertainties are present--(1) the presence of B_4C rods in SEFOR, (2) 5.7 V/O in BeO in SEFOR vs. 8.6% in ZPR-3, and (3) the presence of the gap in the 2-segment design.

The maximum positive reactivity due to sodium loss was measured in ZPR-3 to be 6% for the mockup with no boron carbide present. The presence of boron carbide in SEFOR tends to amplify the hardening of the spectrum contribution and therefore increases slightly the maximum positive effect due to sodium loss. This was observed in Table 3-23. Since no experimental data was obtained on the influence of B_4C on the sodium loss reactivity, a 20% uncertainty is assigned to the difference in calculated sodium loss reactivity effect from that measured with no B_4C . So the maximum positive sodium loss reactivity and uncertainty due to B_4C is:

	+6% with no B_4C ,
	+17% $\pm 2.2\%$ with 3.5% $\Delta k/k$ B_4C loaded,
and	+28% $\pm 4.4\%$ with 7.0% $\Delta k/k$ B_4C loaded.

The sodium loss reactivity was measured in ZPR-3 with 8.6 volume percent BeO while SEFOR contains only 5.7 volume percent. It is estimated that the decrease in BeO will cause an increase of $\leq 10\%$ in the maximum positive sodium loss reactivity effect. The increase is due to a small increase in the spectrum component of the sodium loss reactivity.

As reported in Section 3.7.3, the loss of sodium from the gap region in the 2-segment design was negative. Hence, neglecting the gap in the extrapolation to SEFOR should be conservative.

No additional uncertainty in sodium loss reactivity due to uncertainty in the Plutonium-239 alpha value is introduced on extrapolating from the SEFOR mockup results to SEFOR. The results are normalized to the experimental value; and the fuel concentration, neutron spectrum, and leakage in the SEFOR mockup quite accurately correspond to these quantities in SEFOR.

Conclusion on Sodium Loss Reactivity; After considering the ZPR-3 sodium loss experiment and extrapolation to SEFOR, the maximum positive reactivity due to sodium voiding in the reference design (3.5% $\Delta k/k$ B_4C loaded), including the positive uncertainty band, is $\leq 30\%$.

3.8 OTHER REACTIVITY EFFECTS

3.8.1 Fuel Rod and Channel Bowing

The effects of uniform bowing of the core and bowing of individual fuel rods as a function of radius were calculated. Results were obtained both for unrestrained translation of the rods and for parabolic bowing.

The overall core bowing coefficient for a uniform translation of the rods and channels toward the center of the core* is (based on the structural radial expansion coefficient in Table 3-16):

$$\frac{\Delta k/k}{\Delta R/R} = -0.714$$

For the 20 MW core radius of 44.3 cm, this coefficient can be written as

$$\frac{dk}{dR} = -0.0161 \text{ cm}^{-1} = -5.0\$/\text{cm} = -1.3\text{¢}/\text{mil}$$

* Uniform translation here means a uniform change in density throughout the core with a change in core radius.

The parabolic bowing coefficient, based on the change in radius at the core midplane and assuming the ends of the core remain fixed, is 0.73 times the above translational bowing coefficient. This value was based on perturbation calculations, weighting the parabolic shape of the fuel rod with the square of the axial power distribution.

The effect of a 1 mil translation of an individual fuel rod was calculated at three radial positions in the core. A 100-mil translation of all rods at a given radius was assumed for the calculations in order to provide a sufficient reactivity difference between two successive cylindrical 18-group DEMON calculations. By assuming that the effects were linear and superposable for such a small reactivity change, the reactivity effect per rod per mil of translation at the given radius was obtained.

The results for individual rods are plotted in Figure 3-25 as a function of radial position r/R_0 , where R_0 is the core radius, or 44.3 cm at 20 MW.

3.8.2 Boron Carbide Rod Worth

The design criterion for the maximum calculated worth of a boron carbide rod was 0.70\$. This criterion was met with a rod design containing 312 grams of B_4C . It is unlikely that the calculated worth will be in error by more than 30%, and a 30% underprediction would result in a maximum B_4C rod worth of 0.90\$.

The average worth of a B_4C rod is 0.37\$. The peak-to-average value of the B_4C rod (1.89) assumes that no B_4C rods will be placed in the outer row of fuel rod locations.

FIGURE 3-25. $\Delta k/k$ PER ROD FOR 1 mil BOWING

The average value for replacement of sodium by a fuel rod is 0.24\$. Hence, replacement of an average worth fuel rod with an average worth B_4C rod would result in a total reduction in reactivity of 0.61\$.

For the reference design, the number of boron carbide rods required must correspond to 3.5% in reactivity, or 10.9\$. Therefore, the total number of rods required for the design loading is $10.9\$/0.61\%$, or 18 rods.

The worth of the B_4C rod was calculated from a cylindrical DEMON problem. The rod was assumed to contain 312 grams of B_4C . The 312 grams was obtained by assuming a 36-inch high rod of 0.89-inch inside clad diameter, with a B_4C density of 1.7 g/cm^3 , and the rod filled 50% with B_4C and 50% with steel.

3.8.3 Maximum Worth of All Rods and Dry Wells

The maximum reactivity effects for insertion (or removal) of all rods and dry wells that can be inserted into SEFOR were calculated.

Rods and dry wells that will be inserted into and removed from the SEFOR core include:

Normal Fuel Rod	FRED Dry Well
Guinea Pig Rod	Startup Dry Well
Depleted UO_2 Rod	
B_4C Rod	
BeO Tightener Rod	

The rods and dry wells can be inserted into the minimum critical loading and into the fully loaded SEFOR core. The volume of the minimum critical is about 80% of the volume of the fully loaded core. Hence, it was necessary to compare maximum reactivities for insertion of material into each loading.

The rods listed above can be inserted into a sodium-filled channel and into a channel partially filled with fuel rods and BeO. Furthermore, the sodium-filled channel into which a rod is being inserted could be surrounded by empty or partially filled channels. Hence, to obtain maximum reactivity effects, insertion of rods into all of these possible geometries were considered.

3.8.3.1 Reactivities for Insertion into Fully Loaded Core

The results for insertion of material into the fully loaded core (20 MW configuration) both into a sodium-filled channel and into a channel with six of the seven rod positions occupied are listed in Table 3-24.

TABLE 3-24

MAXIMUM REACTIVITY EFFECT UPON INSERTION
INTO THE FULLY LOADED CORE

	<u>Channel without Fuel</u>	<u>Channel with 5 Fuel Rods and BeO Tightener Rod</u>
Normal Fuel Rod	61¢	39¢
Six Normal Fuel Rods	306¢	-
Guinea Pig Fuel Rod	76¢	49¢
Depleted UO_2 Rod	2¢	-5¢
B_4C Rod	-60¢	-70¢
Steel Rod	13¢	5¢
BeO Tightener Rod	10¢	5¢
Instrumented Assembly (2 Fuel Rods + BeO Rod)	131¢	-
FRED Dry Well	-13¢	-
Startup Dry Well (Filled with Aluminum)	47¢	-

3.8.3.2 Fuel Rod Insertion into Channel
Surrounded by Other Empty Channels

Since the calculated reactivity effect of adding a fuel rod to a sodium-filled channel at the center of the core was greater than adding it to a fuel filled channel, the question arises whether an even larger effect is possible if the sodium-filled channel into which the fuel rod is inserted were surrounded by additional sodium-filled, or partly sodium-filled, channels. Hence, the effect of inserting a rod into the geometry shown in Figure 3-26 was calculated. The maximum calculated effect for this geometry was 55¢ which is less than the 61¢ value shown in Table 3-24.

FIGURE 3-26. GEOMETRY FOR CALCULATING MAXIMUM FUEL ROD WORTH

3.8.3.3 Ratio of Insertion in Minimum Critical to Insertion in Fully Loaded Core

The calculated ratio of the reactivity of a rod in the minimum critical loading to the reactivity of the rod in the core loaded for 20 MW operation is 1.17. This value was calculated by perturbation theory from a knowledge of the power distribution and geometry for the two loadings.

The power distributions for the minimum critical and normal loadings were obtained from DEMON computer problems LTC05 and LTC01, respectively. The relative worth of a fuel rod in these two problems was calculated by the following perturbation expression:

$$\frac{\Delta k_m}{\Delta k_n} = \frac{P_m^2(0) \int_0^{R_n} r_n p_n^2(r) dr}{P_n^2(0) \int_0^{R_m} r_m p_m^2(r) dr} = 1.17$$

where the subscripts m and n refer to the minimum critical and normal loading, respectively, and

- Δk = Reactivity worth of a single rod at the core center
- $P(0)$ = Power density at core center
- $p(r)$ = Radial power density distribution
- R = Core radius

The power densities from problems LTC05 and LTC01 were normalized to the same total power for the two cases.

3.8.4 Flooding of Center Dry Wells

A dry well will be located in the central channel in which the oscillator, FRED, or neutron detectors can be placed. The void diameter in the oscillator and FRED dry well is 1.385 inch; the reactivity effect of flooding this dry well with sodium is +0.23\$. The use of a larger ~2.9 inch diameter dry well for the location of neutron detectors for the initial loading to critical and for noise analysis has been considered. Flooding this dry well would introduce 1.3\$. This value could be substantially reduced by prior filling of unused space with aluminum.

3.8.5 Closing of Void Section of the Gap

The calculated reactivity effect due to lowering of the top segment onto the bottom segment of the 2-segment core at 20 MW, completely closing the void in the gap, is 2.9\$. The value at zero power and at the refueling temperature is less than the 20 MW value because the gap thickness is smaller; this results because of the relative axial expansion of the fuel and the clad, both of which influence the gap thickness. At zero power and 700°F sodium temperature the reactivity effect is ~10% higher than the 20 MW value.

3.8.6 Meltdown Reactivity for One Fuel Channel

The reactivity caused by slumping the top third of the fuel in a central SEFOR channel (6 fuel rods) into the center third of the channel is ~0.65\$.

This estimate was made by comparing the reactivities for an entire center channel containing normal fuel, melted fuel, and sodium as calculated by cylindrical DEMON problems. The change in reactivity caused by slumping the top third into the center third and replacing the top third by sodium was then estimated by power-squared weighting the material reactivities corresponding to their final location axially in channel.

3.9 REACTIVITY CONTROL SYSTEM

3.9.1 Operating Control Requirements

The SEFOR control system must provide operation at 20 MW with the allowable excess reactivity for experiments. Therefore, the operating control requirements include the reactivity necessary to overcome temperature reactivity effects due to Doppler and expansion between zero power at refueling temperature and 20 MW plus the allowable excess reactivity.

A value of 0.5\$ excess at 20 MW has been selected for the requirements listed here. The values for the Doppler and expansion reactivities were listed in Sections 3.5.1 and 3.6.1, respectively. These results are summarized in Table 3-25.

TABLE 3-25

SEFOR OPERATING CONTROL REQUIREMENTS

<u>Contribution</u>	<u>Reactivity Requirement</u>
Doppler	3.0\$
Expansion	2.0\$
Excess at 20 MW	<u>0.5\$</u>
TOTAL	5.5\$

The values in Table 3-25 are for the calculated SEFOR Doppler coefficient, $T \frac{dk}{dT}$, of -0.0085. For a 50% higher Doppler coefficient, ($T \frac{dk}{dT} = -0.013$), the total control requirement would be increased to 7.0\$.

3.9.2 Reflector Control Worth

SEFOR is controlled by the insertion of ten nickel reflector control rods which are located outside the vessel. The reactivity worth of the nickel rods was measured on the SEFOR mockup at ZPR-3. The extrapolation of the ZPR-3 results to the SEFOR reflector control system is described in detail in Reference 1.

The extrapolated SEFOR reflector control worth is 11\$. This value provides sufficient margin for shutdown over the 5.5\$ control requirement for any reasonably possible errors in the prediction of temperature coefficients and control rod worth and in the event that one control rod remains stuck in the maximum reactivity position.

The last rod to be inserted controls the most reactivity; the first rod inserted is the weakest. Two-dimensional x,y calculations indicate that, for a total control system worth of 11\$, the last rod inserted is worth 1.6\$ and the first rod inserted is worth 0.8\$. (From the safety point of view in considering shutdown reactivity, a "stuck rod" corresponds to the first rod inserted.)

The reflector worth as a function of core height for banked rod operation was calculated by means of r,z PDQ calculations. The result is shown in Figure 3-27 for a total reflector worth of 12\$.

The worth of a single control rod as a function of height was obtained from the ZPR-3 experiments on the SEFOR mockup. These results are shown in Figure 3-28.

FIGURE 3-27. CONTROL ROD WORTH VERSUS POSITION

FIGURE 3-28. REFLECTOR WORTH VS. POSITION FOR
2-SEGMENT CORE

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APPENDIXREGION DIMENSIONS AND DATA FOR ATOM DENSITY CALCULATIONS

The data used to calculate atom densities are given in Tables A-1 through A-6. The axial and radial dimensions of each region are given in Tables A-7 and A-8. Material compositions of the radial and axial SEFOR regions outside the core are given in Tables A-9 and A-10.

Steel and sodium atom densities do not include the inclusion of steel spacer fins. The spacer fins were added later in the design and occupy 0.0035 volume fraction. Small differences exist between the fuel clad volume fraction between Table A-2 and Figure 3-5. The value in Figure 3-5 shows an increase in fuel clad thickness from 40 to 41 mils which is closer to the clad as fabricated. The 40 mil value was used in the nuclear calculations. Similarly, the BeO density value of 2.72 (Table A-2) was used in the nuclear calculations whereas 2.77 (Figure 3-5) is closer to the density as fabricated. The effects of these differences on the nuclear characteristics are negligible.

The variation of the radial core dimensions with temperature is controlled by the structure thermal expansion coefficient (Table A-1). The variation in axial core dimensions is governed by the fuel expansion coefficient and the pellet shoulder temperature. It was assumed that all the core expanded like the average fuel rod; the actual radial variation in axial expansion was neglected.

The lower gap, region A7, is defined from the top of the lower fuel segment, including the two UO_2 simulator pellets. The gap differs from the core only inside the fuel cladding, as shown in Figure A-1. The axial

dimension of the gap is determined by a combination of fuel and fuel clad axial expansion. In order to use the same calculational technique that was used in the final analysis of the SEFOR Mockup in ZPR-3, atom densities in the gap were calculated by homogenizing the void, insulator pellets, and all other materials in the gap.

TABLE A-1

LINEAR THERMAL EXPANSION COEFFICIENTS

Material	$\bar{\alpha}$ (70°→350°) (x10 ⁻⁶)	$\bar{\alpha}$ (70°→700°) (x10 ⁻⁶)	$\bar{T}$ (20 MW) (°F)	$\bar{\alpha}$ (70°→20 MW) (x10 ⁻⁶)	($\alpha\Delta T$) _{eff} * (70°→20 MW)
Fuel	2.3		-	-	0.00621
BeO	3.7		760	4.6	
Steel					
Fuel Clad	9.2		800	9.8	
BeO Clad	9.2		760	9.7	
Structure	9.5		760	10.0	

* Average expansion of $33 \frac{13}{16}$ inches, dished pellet fuel with a 40 mil shoulder from 70°F to 20 MW is 0.21 inch. Hence, ($\alpha\Delta T$)_{eff} = $0.21/33 \frac{13}{16}$.

TABLE A-2

CORE VOLUME FRACTIONS AND SMEAR DENSITIES

<u>Material</u>	<u>Volume Fraction</u>	<u>Smear Density</u>
Fuel*	.43164	9.6595**
BeO	.05735	2.72
Steel	.21582	
Fuel Clad (Type 316)	.08110	8.03
BeO Clad (Type 316)	.01217
Side Rods (Type 304)	.03431	
Tightener Sleeve	.07426	
Channel	.01398	
Sodium	.29519	

* Fuel atom fraction $\left(\frac{\text{Pu-239} + \text{Pu-241}}{\text{U} + \text{Pu}} \right) = 18.7\%$.

** Based on: $\frac{\text{wt.fuel dished pellets}}{\text{wt.fuel flat-end pellets}} = 0.9822$

wt. fuel in rod with flat end pellets = 3390 g

Rod I.D. = 0.89 in.; Active length = 33-13/16 in.

TABLE A-3

SODIUM DENSITY

<u>T</u>	<u>P</u>
350°F	0.913 g/cm ³
700°F	0.863 g/cm ³
760°F (20 MW)	0.855 g/cm ³

TABLE A-4

ISOTOPIC COMPOSITIONS

<u>Material</u>	<u>Isotope</u>	<u>Percent</u>
Pu	Pu-239	90.8%
	Pu-240	8.5%
	Pu-241	0.7%
U	U-238	99.78%
	U-235	0.22%

TABLE A-5

STEEL COMPOSITION

<u>Material</u>	<u>Weight Percent</u>	
	<u>Type 316</u>	<u>Type 304</u>
Cr	17	20
Fe	67	68
Ni	13.5	12
Mo	2.5	-

TABLE A-6

ATOMIC OR MOLECULAR WEIGHTS

<u>Material</u>	
Fuel ($\text{UO}_2\text{-PuO}_2$)	270.25
BeO	25.013
Na	22.99
Cr	52.01
Fe	55.85
Ni	58.69
Mo	95.95

TABLE A-7

SEFOR RADIAL REGION DIMENSIONS

<u>Region</u>	<u>Description</u>	<u>Radius (cm)</u>			
		<u>70°F</u>	<u>350°F</u>	<u>700°F</u>	<u>20 MW</u>
R1	FRED Channel	4.214	4.225	4.240	4.243
R2	Core	40.399	40.506	40.651	40.678
R3	Core + Na	47.031	47.156	47.325	47.356
R4	Sodium + Steel	53.34	53.34	53.34	53.34
R5	Steel + Void	58.103	Assumed Power and Temperature Independent
R6	Reflector	73.66			
R7	Void + Aluminum	76.20			
R8	Steel + Void	80.01			
R9	B ₄ C	87.00			

TABLE A-8

SEFOR AXIAL REGION DIMENSIONS

<u>Region</u>	<u>Description</u>	<u>Length (cm)</u>			
		<u>70°F</u>	<u>350°F</u>	<u>700°F</u>	<u>20 MW</u>
A1	Sodium-Steel	Were assumed same for all conditions			15.345
A2	Sodium				* 15.24
A3	Grid Plate				15.505
A4	Lower Reflector	10.716	10.743	10.780	10.780
A5	Insulator	.953	.953	.955	.958
A6	Core	52.942	52.984	53.053	53.272
A7	Gap	5.08	5.187	5.315	5.158
A8	Core	32.942	32.968	33.011	33.145
A9	Insulator	.953	.953	.955	.958
A10	Upper Reflector	10.716	10.743	10.780	10.792
A11	Gap	2.54	2.611	2.690	2.587
A12	Connectors	Were assumed same for all conditions			* 17.903
A13	Neutron Shield				30.48

* The regions outside the axial reflectors have little influence on core parameters, so for expediency in running computer calculations they were assumed to remain unchanged for the various reactor conditions.

TABLE A-9

MATERIAL COMPOSITION OF RADIAL REGIONS

<u>Region</u>	<u>Description</u>	<u>Volume Fractions</u>				
		<u>Sodium</u>	<u>Steel*</u>	<u>Nickel</u> ⁽³⁾	<u>Aluminum</u> ⁽²⁾	<u>B₄C</u> ⁽⁴⁾
R1	FRED Channel	0.7214	0.900 Type 316	--	--	--
R2	Core	Defined Separately				
R3	Core & Sodium	0.3648 ⁽⁵⁾	0.114	Plus 0.5238 times core material V's		
R4	Sodium & Steel	0.5909 ⁽⁶⁾	0.3536 ⁽⁶⁾	--	--	--
R5	Steel & Void	--	$\left\{ \begin{array}{l} 0.12835 \text{ plus} \\ 0.1690 \text{ mild} \end{array} \right\}^{(1)}$	--	0.929	--
R6	Reflector	--	--	0.8489	--	--
R7	Void & Aluminum	--	--	--	0.7058	--
R8	Steel & Void	--	0.2707	--	--	--
R9	B ₄ C	--	0.1435	--	--	0.6753

* Stainless Steel type 304 unless otherwise specified.

(1). Mild Steel was assumed to be 100% Fe with a density of 7.86 gm/cc.

(2). Aluminum was assumed to have a density of 2.702 gm/cc with an atomic weight of 26.98 gms/mole.

(3). Nickel was assumed to have a density of 8.90 gm/cc.

(4). B₄C was assumed to contain boron with a naturally occurring isotopic composition to have a density of 1.70 gm/cc with a molecular weight of 55.29 gms/mole.

(5). This sodium is at the inlet temperature.

(6). The V's are defined for SEFOR at 20 MW. The sodium and steel V's at 700°F isothermal are 0.5928 and 0.3529, respectively, and at 350°F isothermal are 0.5775 and 0.3438, respectively.

TABLE A-10

MATERIAL COMPOSITION OF AXIAL REGIONS

<u>Region</u>	<u>Description</u>	<u>Volume Fractions</u>						
		<u>Sodium</u>	<u>Steel</u>	<u>Nickel</u> ⁽¹⁾	<u>Void</u>	<u>Insulator</u> (UO ₂)	<u>BeO</u>	<u>B₄C</u>
A1	Sodium-Steel	.850	.150					
A2	Sodium	1.000						
A3	Grid Plate	.5185	.4815					
A4,A10	Reflector	.2952	.2158	.4890				
A5,A9	Insulator	.2952	.2158			.4316	.0574	
A6,A8	Core	See Table A-2						
A7	Gap	70°F 20 MW	.2952	.2861 ⁽²⁾	.200	.162	.0574	
			.2952	.2857 ⁽²⁾		.160	.0574	
A11	Gap	.2952	.2158		.4890			
A12	Connectors	.6629	.3371					
A13	Neutron Shield	.444	.156					.400

(1). Nickel density was 8.90 g/cm³

(2). Steel in spacer, spring, and washer was 0.0703 at 70°F and 0.0699 at 20 MW.