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ABSTRACT

The tasks of the Gas-Cooled Fast Breeder Reactor (GCFR) program that are supported by the U.S. Energy Research and Development Administration include: development of GCFR fuel, blanket, and control elements; development of the pressure equalization system for GCFR fuel; an out-of-pile loop facility test program plan; fuels and materials development; fuel-, blanket- and control-rod analyses and development; nuclear analysis and reactor physics for GCFR core design; shielding requirements for the GCFR; reactor engineering to assess the thermal, hydraulic and structural performance of the core and core support structure; development of reactor components including the reactor vessel, control and locking mechanisms, fuel-handling equipment, core support structure, shielding assemblies, main helium circulator, steam generator, and auxiliary circulator; the development of a helium circulator test facility; and reactor safety, including an in-pile safety evaluation program.

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I. INTRODUCTION

The various tasks of the Gas-Cooled Fast Breeder Reactor (GCFR) program sponsored by the U.S. Energy Research and Development Administration are discussed in the following sections of this quarterly progress report for the period August 1, 1975 through October 31, 1975.

The GCFR Utility Program, which is supported by a large number of electric utility companies, rural electric cooperatives, and General Atomic Company, is primarily directed toward the development of a GCFR demonstration plant. This utility-sponsored work and the ERDA-sponsored work are complementary.

To establish the basis for the design of the GCFR fuel, blanket, and control element assemblies, analytical, experimental, and fabrication development is being done under the core-element development task. Analytical methods development for structural and thermal-hydraulic analyses is discussed. The results of structural analysis of the fuel-assembly components and thermal-hydraulic analysis of the blanket element during low power are presented. Current progress on the rod-spacer interaction tests, fuel-element seismic and vibration test planning, and the development of assembly fabrication techniques is also presented. The various subtasks of the core-element development and the work accomplished during this reporting period are discussed in Section II.

The technology to support the design and construction of the pressure-equalization system for GCFR fuel is being developed. This includes the development of analytical models and computer codes that will be verified by test programs and testing of materials and seals and developing fabrication processes for the pressure-equalization system, as discussed in Section III.

To demonstrate the ability of the GCFR fuel-, control-, and blanket-assembly designs to meet design goals, and to verify predictions of analytical models, a series of out-of-pile simulation tests are to be performed. The test emphasis will be on obtaining thermal-structural data for steady-state, transient, and margin conditions using electrically heated rod bundles in a dynamic helium loop as discussed in Section IV.

In the fuels and materials development program, thermal-flux and fast-flux irradiation programs are being conducted to establish conditions and design features specific to GCFR fuel rods, such as vented fuel, fission-product traps, and surface-roughened cladding. In addition, a test program is being conducted of GCFR cladding specimens, both smooth and surface-roughened, to determine how materials behave under irradiation. These fuels and materials tests and analytical studies and the results to date are presented in Section V.

Under the fuel-rod engineering task, performance of the fuel and blanket rods under steady-state and transient conditions is being evaluated to determine performance characteristics, operating limits, and design criteria. In addition, surveillance of the fuel-rod and blanket-rod technology of other programs is being carried out. These studies are presented in Section VI.

The verification and validation of the nuclear design methods that will be applied to the GCFR core design are the objectives of the nuclear analysis and reactor physics task. For that purpose a critical experiments program is being carried out. The critical assembly design, analysis, and methods development are presented in Section VII.

Verification of the physics and engineering analytical methods and data for the design of the GCFR shields and an evaluation of the effectiveness of various shield configurations are being conducted under the shielding requirements task. The results of radial shield analyses and the work being done on structural analysis are presented in Section VIII.

To assess the thermal-hydraulic performance of the GCFR reactor core, analytical methods and models are being developed and utilized to define operating strategies. These, together with an evaluation of the capability of the PCRV internal structures to provide post-accident fuel containment and the development of GCFR plant control systems, are discussed in Section IX.

Evaluation and development now in progress on the main components of the GCFR, including the reactor vessel, control and locking mechanisms, fuel-handling development, the core-support structure, shielding assemblies, the main helium circulator, the steam generator and the auxiliary circulator are presented in Section X.

Development of a test facility for qualification testing of the main helium circulator is discussed in Section XI. Included under this task are the responsibility for evaluation studies of alternative test facility concepts and preparation of specifications for the selected facility, as well as the responsibility for the final design, construction and checkout of the facility.

The reactor safety task, which is discussed in Section XII, is to study the safety aspects of the GCFR using logical probabilistic methods to determine the probabilities associated with accident initiation and progression sequences.

The Gas Reactor In-Pile Safety Test (GRIST) program is being studied as a potential follow-on to the analytical and experimental programs covering design-basis accidents. The objective of GRIST would be to provide information related to beyond-design-basis accidents, particularly the behavior of melted cladding and fuel. Progress in test assembly analysis and design is discussed in Section XIII.

II. CORE-ELEMENT DEVELOPMENT (189a No. SU006)

2.1. FUEL- AND CONTROL-ASSEMBLIES ANALYSIS

The analytical basis, in conjunction with experimental evaluation, for the design and development of the GCFR fuel and control assemblies is being developed. Because complete prototype in-pile tests cannot be conducted, a strong analytical base, supported by development tests, is required to design the core assemblies. The current emphasis is on developing an adequate steady-state and transient analysis capability and performing both thermal-hydraulic and structural steady-state and transient analyses in order to provide a basis for the assembly design criteria and for specific test requirements.

During the previous quarterly period, formal solutions were obtained for the FLOMAX thermal-hydraulic code for the inner regions near spacers for adiabatic flow, and the coding of the outer solutions was in progress. Several COBRA-IV modifications were verified for correctness, and the code was used to perform several thermal-hydraulic analyses. Based on the COBRA results, bowing distortions of the duct and the guide tubes of the control-rod assembly were determined.

During the present quarterly period, the programming of the first-order, outer temperatures in FLOMAX was completed, the subchannel temperatures were compared with COBRA results, and the programming of the first-order, outer flow rates was initiated. Formal inner solutions were obtained for the complete thermal-hydraulic problem, but the coding of these solutions was not begun. The thermal-hydraulic analyses for this period included the calculation of subchannel and rod surface-temperatures in an assembly with a dilated duct, and the determination of assembly pressure drops by hand

calculations and with the COBRA code. The dilation resulted in increased thermal gradients in the edge rods of the assembly. The hand calculations of assembly pressure drops were found to give results comparable with COBRA.

2.1.1. Development of the Rod-Bundle Thermal-Hydraulic Analysis Code FLOMAX

The primary mathematical problem of rod-bundle codes is to determine for each subchannel i , and as a function of the axial coordinate x , the mass velocities $G_i(x)$, the pressures $P_i(x)$, and the temperatures $T_i(x)$. In FLOMAX this is done by expanding these variables into the asymptotic series,

$$\begin{aligned} G_i(x) &\sim G_{oi} + \mu G_{1i}(x) + \nu g_i(\xi) \quad , \\ P_i(x) &\sim P_o(x) + \mu P_1(x) + \pi p_i(\xi) \quad , \\ T_i(x) &\sim T_o(x) + \mu T_{1i}(x) + \tau t_i(\xi) + \mu^2 T_{2i}(x) \quad , \end{aligned} \tag{1}$$

where the upper case letters denote what are called outer solutions (valid away from spacers and transitions), and the lower case letters denote the inner solutions (valid near the spacers and transitions); the Greek letters denote small parameters, and the stretched coordinate is $\xi = x/\epsilon$. The significance of these terms will be clarified subsequently when comparing results. The present status of FLOMAX is: (1) the solutions G_{oi} , $P_o(x)$, $T_o(x)$, and $T_{1i}(x)$ have been programmed and are now being verified through sample calculations, and (2) the $G_{1i}(x)$'s and $P_1(x)$'s are being programmed. The results indicate that the functions $T_{2i}(x)$ are not required, so that the functions left to program for completion of the code are $g_i(\xi)$, $p_i(\xi)$, and $t_i(\xi)$.

The results of the above solutions were compared with COBRA-IV results for several sample problems. First it was necessary to compare the turbulent mixing models in FLOMAX and COBRA, which are not identical. In COBRA the turbulent mixing input is restricted to one correlation for all parts of the rod bundle, whereas in FLOMAX different mixing coefficients can be

specified for different parts of the assembly. However, it was found that for the special case in which all subchannel friction factors are given by the same correlation, of the form $f_i = A \text{Re}_i^n$, near equivalence between the mixing models was possible.

Calculations were made for a hypothetical bundle consisting of two subchannels, one small (subchannel 1) and one large (subchannel 2), with the same amount of heat added to each of them. The flows G_{o1} and G_{o2} , and the pressure drop $P_o(0) - P_o(L)$, are summarized in Table 2.1 for the case of zero heat addition.

The agreement between the flow rates is adequate, but in both problems FLOMAX predicted a slightly lower pressure drop than COBRA. This difference will not improve by including $P_1(x)$, because this function is zero for adiabatic problems. Further calculations will be done to establish the correctness of the pressure drop calculations in FLOMAX.

Subchannel temperatures were calculated for a uniform axial power profile, and the results are shown in Fig. 2.1. The temperatures calculated in FLOMAX as $T_i(x) = T_o(x) + \mu T_{1i}(x)$ are shown as solid lines, and the COBRA calculation points are shown as circles up to $x/L = 0.3$ and thereafter as dashed lines. For this problem the outer region is from about $x/L = 0.25$ to 1.0, and it can be seen that there is good agreement between the temperatures calculated with the two codes. Subchannel 1 has the smaller area and flow rate and therefore the higher temperature. If it is assumed that the COBRA results are exact, then it may be possible to achieve better agreement in the outer region by including the functions $T_{2i}(x)$. However, the temperature in subchannel 1 is very sensitive to the degree of turbulent mixing. If the mixing factor in FLOMAX is increased by 10%, then $T_i(x)$ for $i = 1$ is reduced by 20°C as shown, and $T_i(x)$ for $i = 2$ is increased by 2°C. The degree of turbulent mixing in a rod-bundle is known, at best, only to within $\pm 50\%$; therefore the FLOMAX outer temperature solution is adequate with only the $T_o(x)$ - and $T_{1i}(x)$ -terms included.

Table 2.1
COMPARISON OF COBRA AND FLOMAX RESULTS

Friction Correlation	Code	G_{o1} (kg/s·m ²)	G_{o2} (kg/s·m ²)	$P_o(0) - P_o(L)$ (kPa)
f = 0.02	FLOMAX	413.3	489.2	132.2
	----	---	---	---
	COBRA	415.1	489.0	134.5
f = 0.184 Re ^{-0.2}	FLOMAX	360.9	495.1	128.9
	----	---	---	---
	COBRA	363.0	495.0	131.0

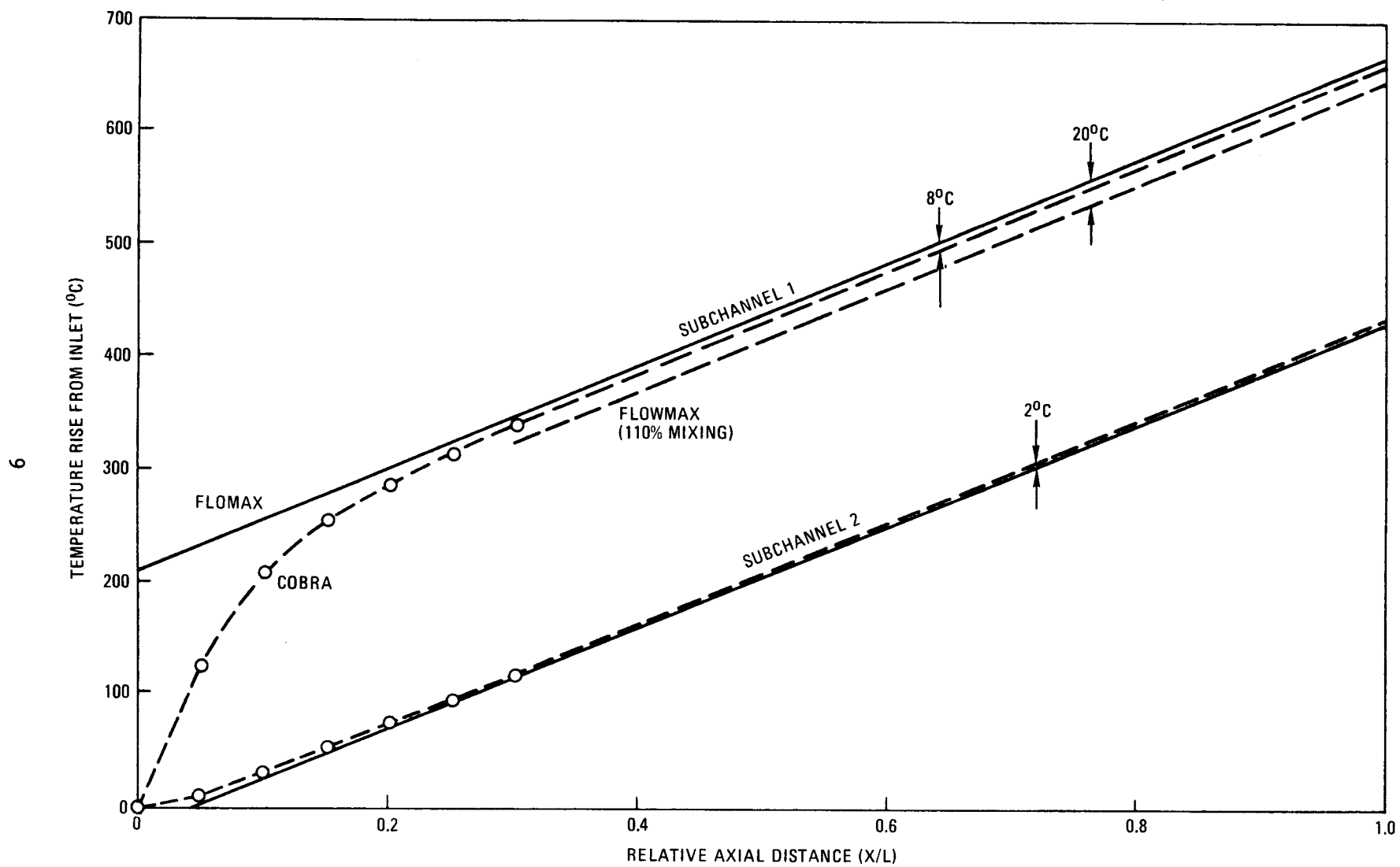


Fig. 2.1 Comparison of FLOMAX outer subchannel temperatures with COBRA-IV results

The inner region of this problem is the region from $x/L = 0$ to $x/L \approx 0.25$, and in this region FLOMAX does not agree well with the COBRA results. This is because the outer solutions cannot satisfy any initial conditions. In FLOMAX the inner solution $t_i(\xi)$ will correct for the $T_1(x) - \nu T_{1i}(x)$ solutions near $x = 0$. The functions $t_i(\xi)$ will decay exponentially as $e^{-x/\lambda L}$. The dimensionless distance λ was found to be approximately $\lambda = 0.045$, so that in $5\lambda = 0.225$ the $t_i(\xi)$ contribution will have vanished, which is consistent with the results in Fig. 2.1. The formal solution of $t_i(\xi)$ was obtained during this quarter, but the programming is yet to be initiated. The pressure drop for the heat problems was larger than for the adiabatic problem, as it should be. However, the agreement between the two codes was reduced. Agreement is expected to improve with the inclusion in FLOMAX of the function $P_1(x)$, now being programmed.

2.1.2. Thermal-Hydraulic Analysis

The analysis to determine the effect on the fuel rod and subchannel temperature distributions of the dilation of the fuel assembly duct wall was completed. A 1/12 section of a fuel element was used as the model for the COBRA-IV subchannel analysis code. A base case was established in which the rod/wall gap was 40% of the nominal rod/rod gap. For this case the center rods have a spatially average temperature of 604°C . There is an 11°C temperature difference between the center and side subchannels, and a 17°C temperature difference across the rods in the outer row.

The magnitude and axial profile of the duct wall dilation have been previously determined based on current irradiation creep data (Ref. 1, p. 18). The lateral shape of the dilation is such that the maximum value of the dilation occurs at the midpoint of the duct side wall, and there is no dilation at the duct corner. The dilation causes the subchannels nearest the midpoint of the side wall to increase in area by over a factor of two.

Figure 2.2 gives the temperature distributions resulting from these area changes. The increased subchannel areas are also shown on this figure.

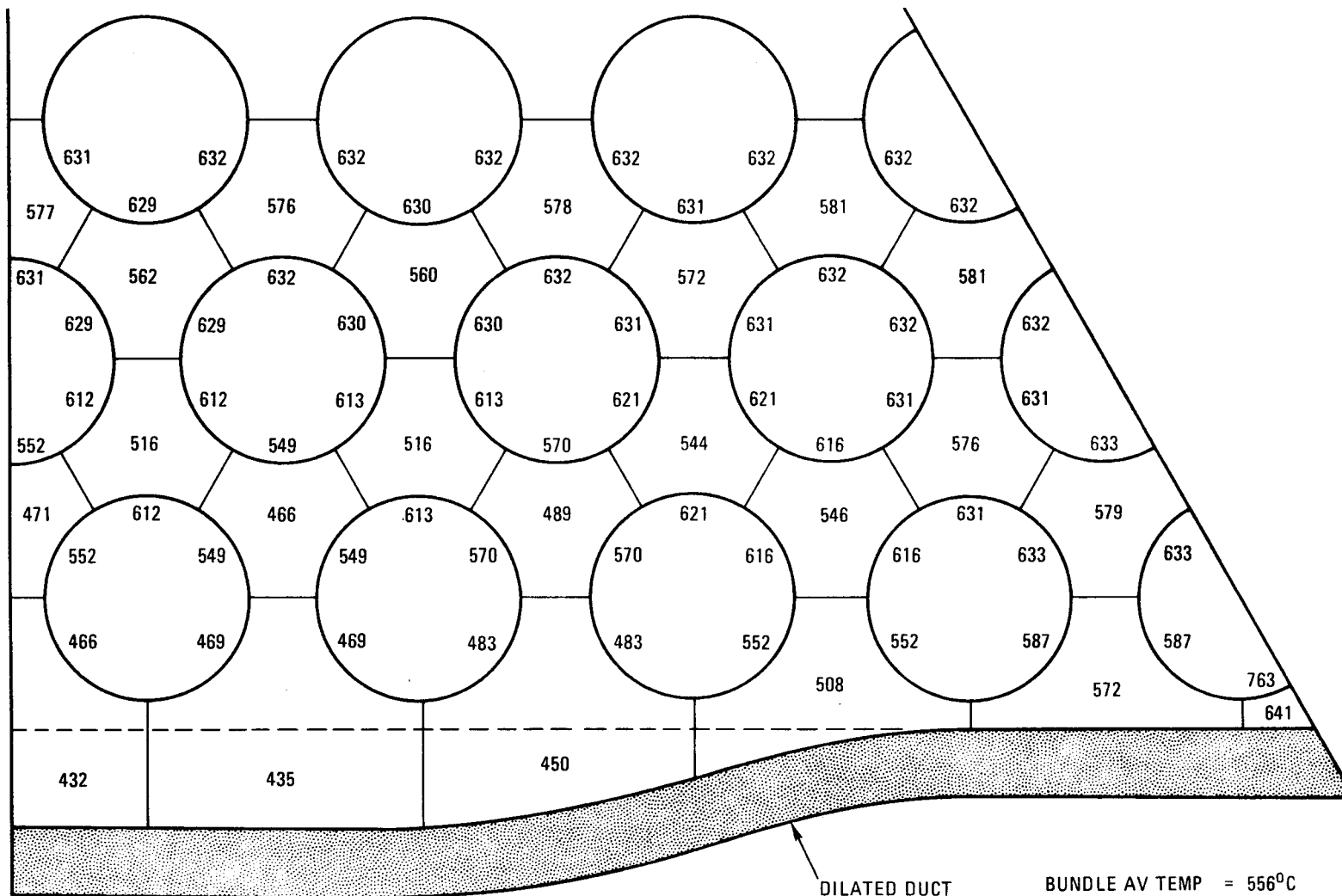


Fig. 2.2 Temperature distributions with duct dilation

Because of the increase in the subchannel area, there is additional coolant flow near the duct wall and less in the center of the element. Because of this increased flow in the side subchannels, a temperature difference of about 144°C is created across the outer row of rods, compared to the 17°C difference that existed for the base case; also, the assembly pressure drop was reduced by $\approx 17\%$ because of the increased flow in the region of reduced frictional resistance. If the same pressure drop is maintained as in the base case, mass flow is increased by 10%. With this increased mass flow, the center rod temperatures are within a few degrees of those of the base case, but there is still a 133°C temperature difference across the outer row of fuel rods. This problem will be analyzed further, including circumferential conduction in the rods.

An analysis was also performed to determine the adequacy of the pressure drop formula for calculating the assembly pressure drop. The formula was first derived from the basic differential equations, which led to the following result (neglecting spacer losses),

$$\Delta p_c = q_m \left(f \frac{L}{D_H} + \alpha \frac{\Delta T_c}{T_m} + \frac{\rho_m}{\rho_{in}} C_{in} + \frac{\rho_m}{\rho_{out}} C_{out} \right), \quad (2)$$

where

- Δp = pressure drop,
- q = velocity head,
- f = friction factor,
- T = absolute temperature,
- ΔT = temperature rise,
- ρ = density,
- C = loss coefficient,

and where the subscripts have the meanings,

- m = mean value over length L ,
- c = core,
- in = inlet,
- out = outlet.

The derivation showed that Eq. 2 is valid only if f and the flow area do not change axially. Thus, for the GCFR fuel assemblies that have smooth and rough axial sections, it was necessary to calculate the frictional losses in sections:

$$q_m^f \frac{L}{D_H} \equiv \sum_j \bar{q}_j^f f_j \ell_j / D_{H_j} \quad , \quad (3)$$

where j refers to the sections. Another point concerns the coefficient α of the acceleration pressure-drop term. If one considers the overall irreversible pressure losses from plenum to plenum, then $\alpha = 1$. However, if one considers the static pressure drop within the assembly only, as in sub-channel analyses, then $\alpha = 2$.

Using Eqs. 2 and 3 the fuel assembly static pressure drop was calculated and compared with the results of COBRA-IV. In the case of an infinite array of rods, with the rod pitch adjusted to give equal flow area, the friction factor to be used with Eq. 2 in the rough-rod section is just the rough friction factor. In the case of a finite array of rods with an assembly duct, the friction factor of the rough section was determined from a perimeter weighting of the smooth and rough friction factors. The results are summarized below, where the numbers are Δp_c in kPa:

<u>Infinite Array</u>		<u>Fuel Assembly With Duct</u>	
<u>Equation 2</u>	<u>COBRA</u>	<u>Equation 2</u>	<u>COBRA</u>
188.9	184.1	213.7	214.4

These results agree within 1%, so it was concluded that the pressure drop formula (Eq. 2) is adequate, provided that the frictional losses are calculated correctly, (Eq. 3) and that the appropriate coefficient α is used.

2.1.3. Element Structural Criteria

The Westinghouse document, #WARD-3045T2C-5, "Preliminary LMFBR Core

Structural Design Criteria," has been reviewed with respect to its applicability to the GCFR fuel-, blanket-, and control-element components, exclusive of rod cladding. It was found to be very similar to the GE cladding criteria, reviewed and modified for GCFR use under subtask 6.3 ("Design Criteria"). Subsequent to the two reviews, it has been determined that a consolidation of the cladding criteria and the remaining core components criteria would be advantageous. Thus, the first attempt has been to modify the WARD core component criteria into the form of the GE cladding criteria. This requires only the addition of unique safety factors on stress, strain and cumulative damage limit equations. Thus, a set of criteria for each operating condition (normal, upset, emergency, and faulted) is no longer required. Rather a general set of rules can apply to the cladding and the core component criteria alike. The actual structural limits are determined separately for each component by application of the safety factors.

2.2. BLANKET ASSEMBLY ANALYSIS

The purpose of this task is to develop and verify the analytical techniques for blanket assembly analyses. These analytical techniques will be applied to obtain preliminary and final designs of the blanket assembly and to determine the design margin requirements. The task includes the thermal-hydraulic, structural, and dynamic analyses and the evaluation of handling and shipping of the blanket assembly.

During the previous reporting period, a review of the analytical methods for transient thermal-hydraulic analysis of the blanket assembly was initiated. Also, a scoping study was initiated to investigate an alternative design of the blanket assembly.

A preliminary steady-state thermal-hydraulic analysis of a 61-rod blanket assembly with a small pitch-to-diameter ratio and wire-wrapped spacer was initiated. The version of the COBRA-IV code available at GA does not contain the wire wrap option. Hence, the wire wrap mixing was simulated by increasing the turbulent mixing factor. Using the results of the thermal-hydraulic analysis, a rod bowing analysis was initiated.

2.2.1. Peak Powers in the Radial Blanket

In the last quarterly report,⁽¹⁾ the power distribution in the thorium blanket for four years residence time was reported. The peak power of 72.1 W/cm^3 occurred in the first row at the end of the second cycle. This analysis was repeated for six years' residence time. Also, a comparative study was made to estimate relative powers in uranium and thorium blankets. Table 2.2 summarizes the peak powers expected in uranium and thorium blankets for four and six year residence times. These powers include the uncertainties associated with nuclear calculations (1.15 for thorium and 1.08 for uranium) and also include correction for geometrical location of three blanket elements nearer to the core. Based on these powers, the permissible rod diameters to limit the peak linear power to 475.7 W/cm (14.5 kW/ft) were calculated and are included in Table 2.2.

2.2.2. Sizing Study

The reference design of the GCFR radial blanket⁽²⁾ was based on the management scheme that left the blanket assemblies in the first row for three years without rotation. This resulted in higher rod powers and consequently a large number of rods (127 per assembly) at a large pitch/diameter ratio (1.145).

In light of the management scheme proposed in the last quarterly report,⁽¹⁾ the blanket design can be modified in two ways: first, the number of rods per assembly can be decreased thus increasing the rod diameter (not to exceed the maximum given in Table 2.2); second, the pitch/diameter ratio can be reduced to increase the volume fraction of the fertile material in the radial blanket. This will increase the breeding ratio. Figure 2.3 shows the relation between the pitch/diameter ratio and the resulting increase in the volume fraction of the fertile material. A value of $p/d = 1.05$ can be considered as a lower limit because beyond this the hot spot factors will be excessive.

Table 2.2
PEAK POWERS AND THE MAXIMUM PERMISSIBLE ROD DIAMETERS
IN THE RADIAL BLANKET OF THE GCFR

	Thorium Blanket		Uranium Blanket	
	Residence Time (Years)			
	4	6	4	6
Peak Power (W/cm ³)	87.9	129.2	132.0	194.2
Maximum Rod Diameter (mm)	27.45	22.83	22.61	18.85

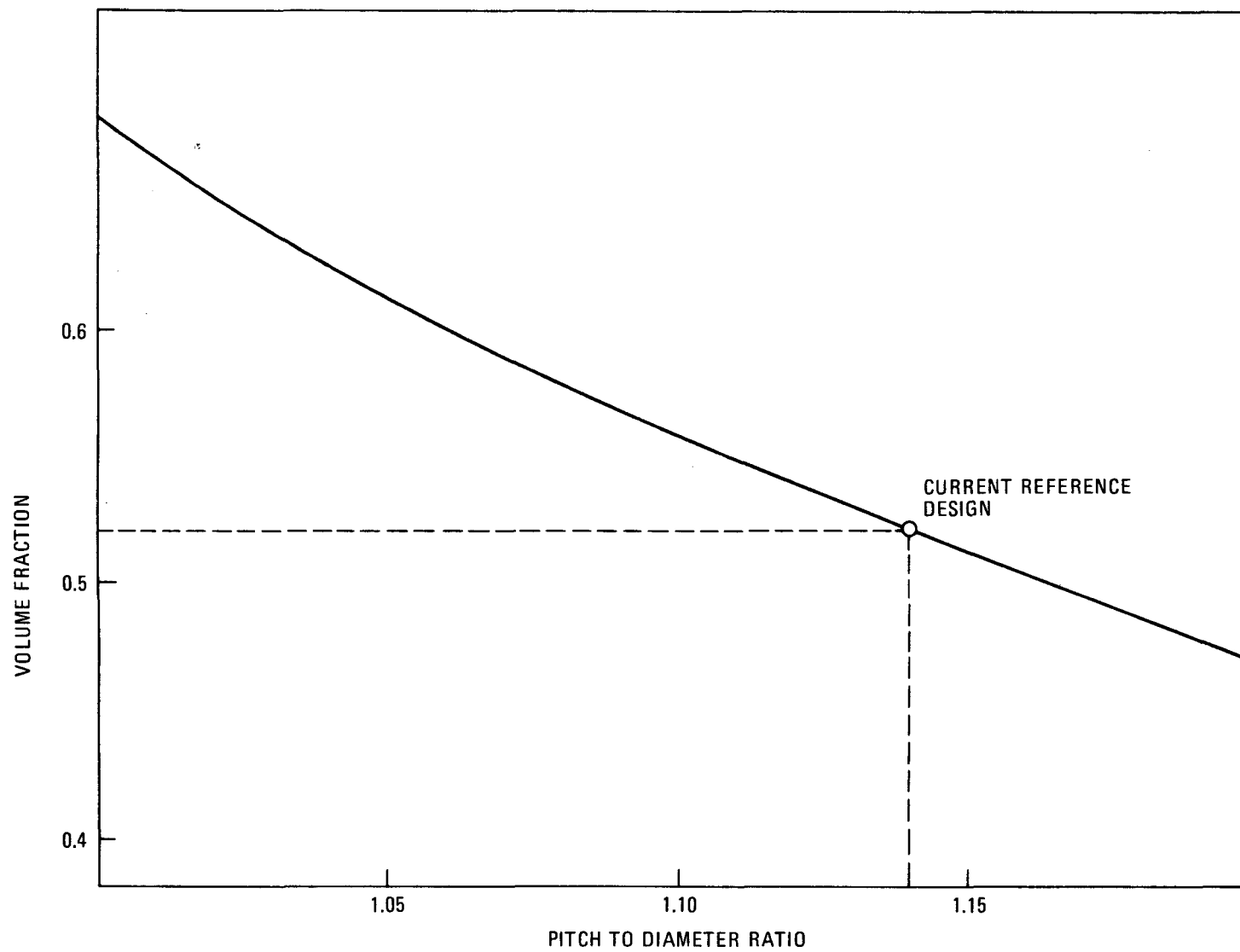


Fig. 2.3 Sizing study for GCFR radial blanket

It is desirable to have a configuration of the blanket assembly that will be applicable to either a thorium or a uranium blanket. With this limitation, the blanket configurations become:

<u>Residence Time (years)</u>	<u>Rods/Assembly</u>	<u>Rod Diameter (mm)</u>
4	61	19.76
6	91	16.24

Economic analysis of the GCFR radial blanket shows that a configuration of 61 rods per assembly for four years residence time is more economical than 91 rods per assembly for six years. Hence, the following configuration is considered most desirable:

Rods/assembly	61
Pitch/diameter ratio	1.05
Rod diameter	19.76 mm

For the small pitch/diameter ratio under consideration, the spacer arrangement of the reference design is not suitable. Hence, a wire wrapped spacer with a wire diameter of ≈ 1 mm will be used. The various wire wrap arrangements are under study.

2.2.3. Preliminary Thermal-Hydraulic Analysis of the Radial Blanket Assembly

A half bundle of the radial blanket assembly was modeled on the COBRA-IV code for the following configuration:

Diameter of the rods	19.76 mm
Pitch/diameter rods	1.05
Number of rods/assembly	61
Edge spacing	100%

The axial and radial power distributions reported in Reference 1 were used, Since the gas cooled version of the COBRA-IV code available at GA

does not contain the option for wire wrapped spacers, the wire wrap mixing was simulated by increasing the turbulent mixing factor to 6%. This represents the interchannel mixing caused by a wire at a pitch of 125 mm.

2.2.3.1. Hot Spot Factors for Blanket Assembly. Although no detailed calculations for hot spot factors for the blanket assembly have been performed, these factors are expected to be much larger than for the fuel assembly. The subfactor that causes the increase in the film hot spot factor is the variation of circumferential cladding temperature. This subfactor for $p/d = 1.05$ and a wire wrapped spacer could be as high as 2.0. Until detailed calculations are performed, the following values of hot spot factors will be used in the thermal-hydraulic analysis of the blanket assembly:

Bulk hot spot factor	1.3
Film hot spot factor	2.25
Cladding hot spot factor	1.2

2.2.3.2. Study of Edge Spacing. The selection of suitable edge spacing presents a unique problem in the radial blanket due to steep radial power gradients. It is desirable to have a minimum temperature gradient in the corner and peripheral rods without excessive over-cooling of the peripheral channels. Table 2.3 shows a COBRA study of the temperature gradients across peripheral and corner rods for 50% and 100% edge spacings (percent of rod-to-rod spacing). Since it is desirable to slightly over-cool the corner channel a 100% edge spacing will provide the most suitable spacing.

2.2.3.3. Temperature Degradation. Due to the limited mixing associated with the wire wrapped spacers, the large radial power gradient, and the large hot spot factors expected at the small pitch/diameter ratio considered here, the radial blanket assemblies must be over-cooled to limit the hot spot cladding temperature to 700°C. According to conservative estimates, the bulk temperature increase in the blanket assembly will be about 110°C (~ 50% of that across the fuel assemblies). Since the power produced in the radial blanket is about 3.2% at the end of the equilibrium cycle, this represents a 7°C degradation of the core outlet temperature.

Table 2.3
STUDY OF EDGE SPACING

Edge Spacing (%)	Temperature Difference ^a (°C) Across the Rod at x/L = 0.6 in Peripheral and Corner Channels					
	Rod Number ^b					
	21	22	23	27	31	32
50	-42.3	-42.3	+27.6	+13.4	+5.17	-19.8
100	-62.5	-61.4	-15.6	-15.6	-10.8	-28.4

^aA negative temperature gradient indicates edge or corner temperature less than inside temperature.

^bSee Fig. 2.4.

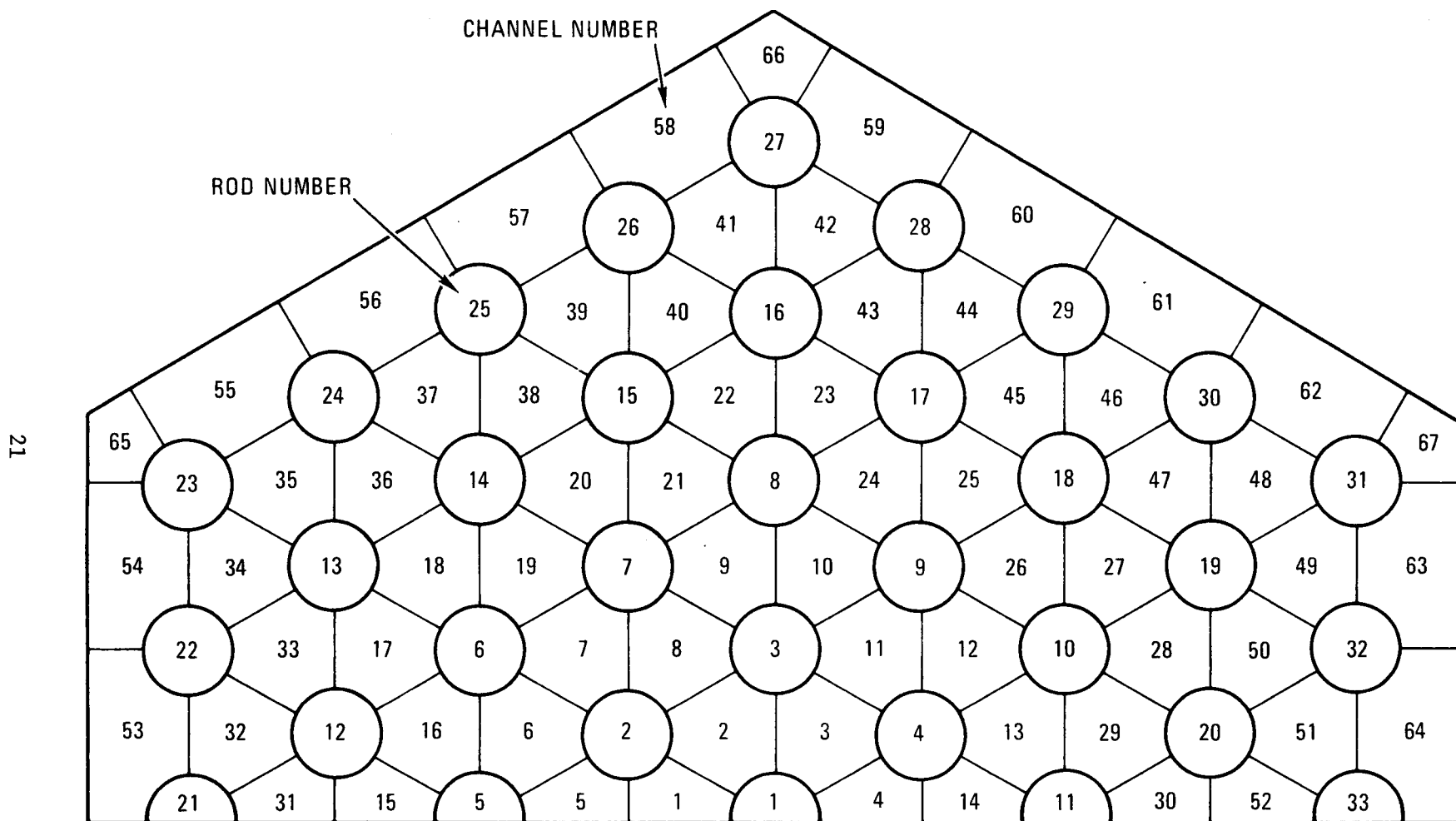


Fig. 2.4 COBRA model of GCFR radial blanket

2.2.3.4. Temperature Distribution in the Radial Blanket. Figure 2.5 shows the axial distribution of the nominal coolant and cladding temperature in the hottest channel for an inlet temperature of 323°C and mixed mean outlet temperature of 434°C. Figure 2.6 shows the radial distribution of nominal coolant temperature at outlet from the blanket assembly. The temperature distributions obtained in this analysis are being used for structural analysis to determine the rod bowing due to thermal gradients.

2.2.4. Blanket Element Structural Analysis

A preliminary study of the bowing characteristics of large blanket rods has been initiated. Temperature gradients across the rods for 50% and 100% edge spacing are discussed in Section 2.2.3. The temperature gradients result in thermal deflections of the rod, or if restrained by some spacer arrangement, in reaction forces on the cladding. In addition to the increased rod size a constraining system of wrapped wire was also included in the study.

The finite element code CRASIB was used to analyze the deflections and reaction loads of a 42 beam element mode. Two rod diameters were analyzed, 2.45 cm and 2.00 cm. The rods were assumed to be rigidly fixed at the top support. Locations of lateral support were arranged to correspond with the particular wire wrapping scheme. The 2.45 cm OD rod had alternating sided support points at intervals of 7.62 cm. Two different wire wrap patterns were explored in the case of the 2.00 cm OD rod. These are shown in Figs. 2.7 and 2.8. The first corresponds to a wire diameter equal to the diametral gap between the rods; the second to a wire diameter of 1/2 this size. For the most part these supports are also one sided. Since CRASIB only analyzes axially symmetric supports, an iterative procedure was used to determine the active supports (defined below). Each problem was run with all the support locations present. The resultant reaction forces and displacements were used to determine if the displacement were toward or away from the one sided constraints. Displacements toward a one sided constraint made it an active support. Appropriate supports were retained, and the

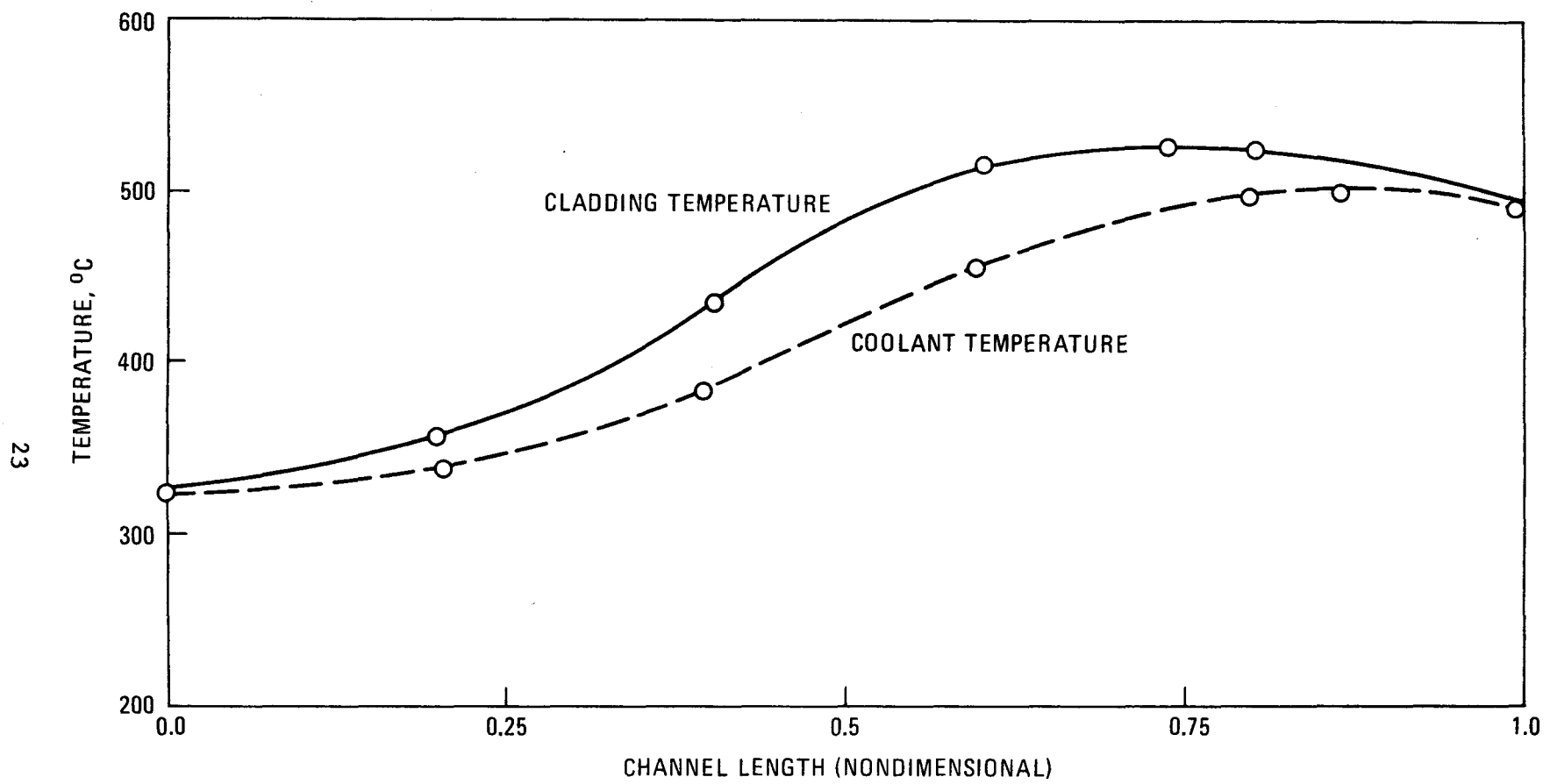


Fig. 2.5 Nominal coolant and cladding temperature distributions in hot channel for a 6l rod wire wrapped GCFR radial blanket assembly

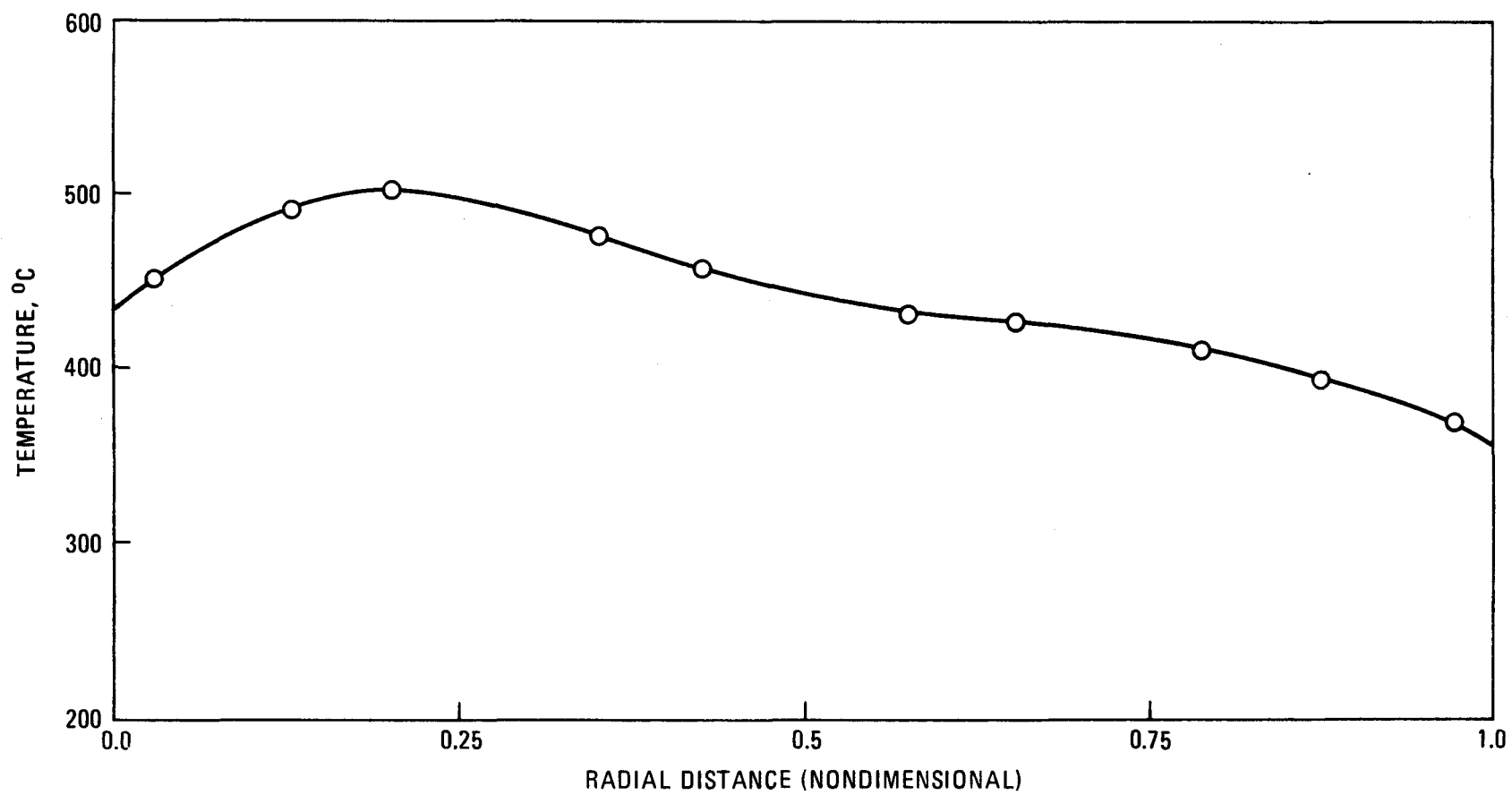


Fig. 2.6 Radial distribution of the nominal coolant temperature at outlet from the radial blanket assembly

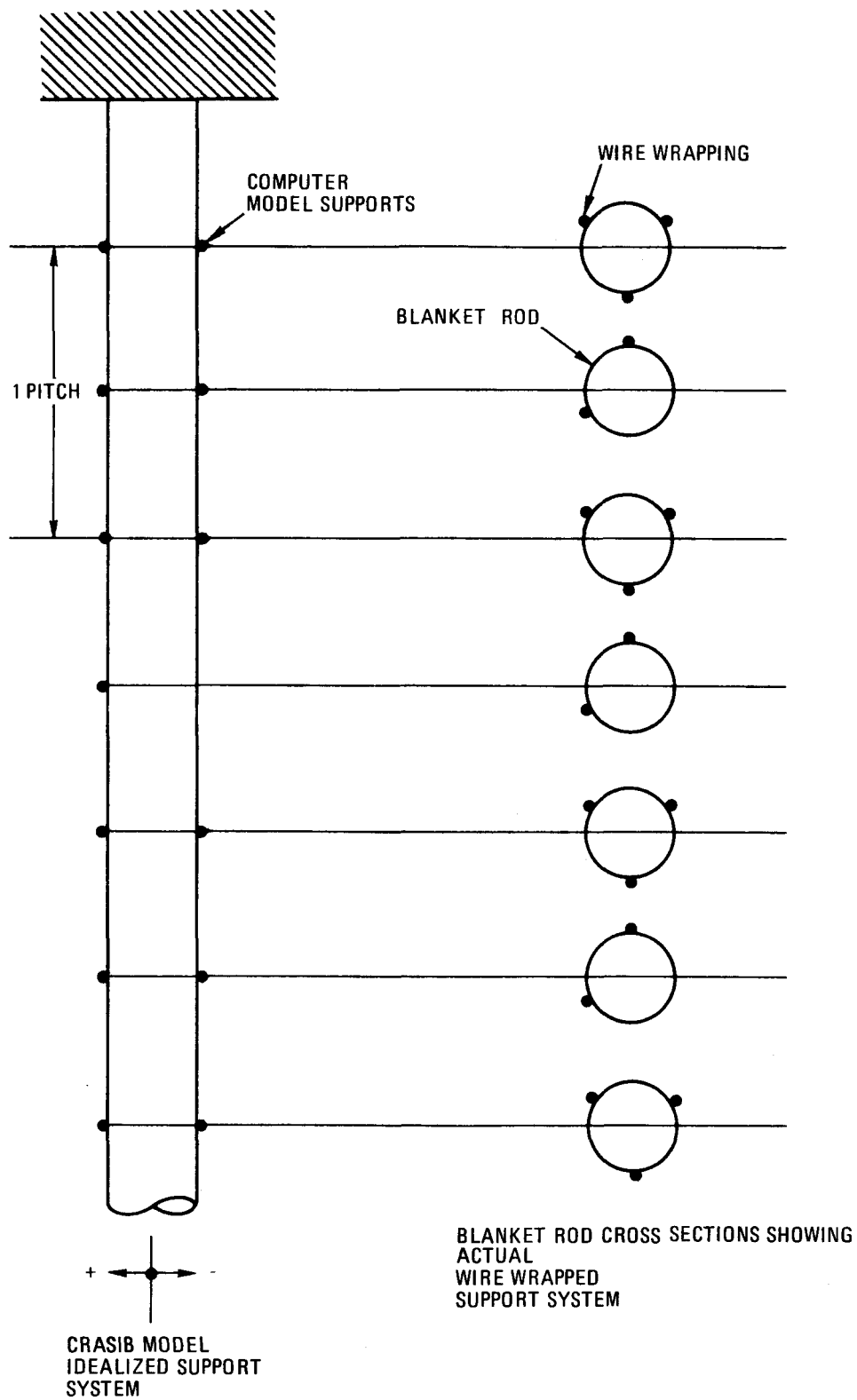


Fig. 2.7 Support arrangement for small wire diameter option of wire wrapped blanket rod (61-rod bundle)

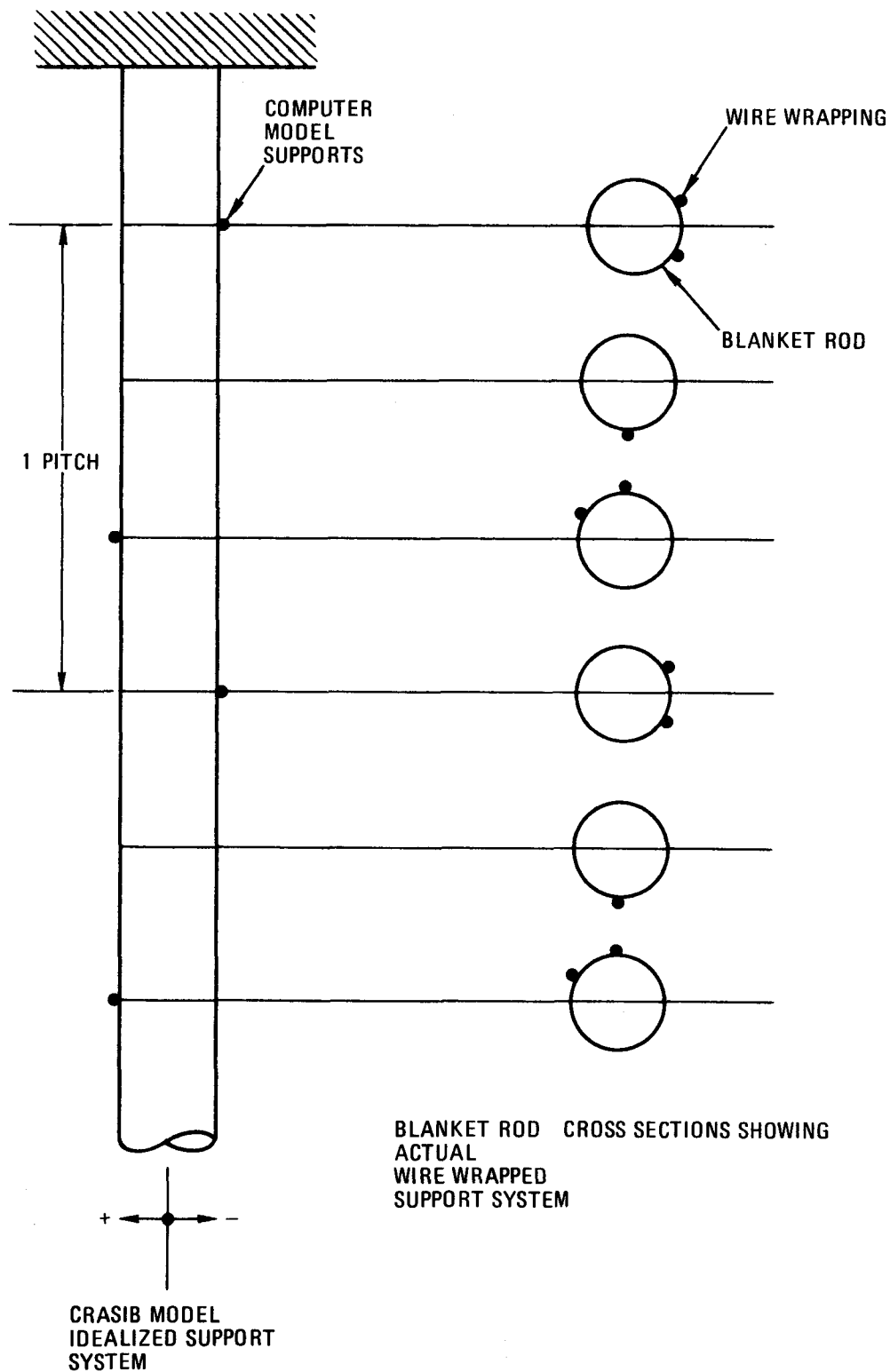


Fig. 2.8 Support arrangement for large wire diameter option for wire wrapped blanket rod (61-rod bundle)

problem was rerun. The procedure was continued until all the active supports were identified. This usually took about four or five iterations.

For this analysis, only the elastic response of the rod was considered (i.e., no creep or swelling because blanket fluence does not exceed about 5×10^{22} nvt). In all cases the bottom of the rod was the most critical because both the maximum displacements and reaction forces occur there. Results are presented in Table 2.4. The three columns under "Reaction Loads" refer to the bottom three support points in descending order (i.e., no. 3 is the lowest).

From the results it can be seen that none of the three designs is superior in producing reaction loads. The 2.00 cm large wire option, however, exhibits the largest free end displacement. Future efforts will be directed toward obtaining an optimum balance between the lowest reaction loads and the largest displacements permissible by varying the position of the lower supports. Additionally, since the reactions are produced by adjacent rods, which are themselves flexible, a more refined analysis will be made considering semi-rigid supports and their movements due to adjacent rod displacement.

2.3. ASSEMBLY MECHANICAL TESTING

2.3.1. Rod/Spacer Interaction Tests

The purpose of the rod/spacer interaction tests is to evaluate the interaction between the rod and the spacer in the ranges of temperature and impurity levels (primarily water and hydrogen) expected in the GCFR helium coolant. These interactions arise from the relative motion between the rod and the spacer due to temperature variations during reactor transients. This interaction leads to friction and wear and possibly to adhesion between the rod and the spacer.

Initially, the combinations of pressed spacers with convex contact surfaces and smooth or ribbed rods were tested. Both spacers and rods were

Table 2.4
SPACER/CLADDING REACTION LOADS

Case	Reaction Loads (Newtons)			Free End Displacement cm
	1	2	3	
2.45 cm OD	39.9338 N	123.0930 N	118.3881 N	0.0017 cm
2.00 cm OD Large wire	48.2877 N	120.6945 N	85.1327 N	0.0073 cm
2.00 cm OD Small wire	28.0103 N	103.4527 N	90.3167 N	0.0028 cm

of type 316 stainless steel. Adhesion was observed in tests on smooth rods in an environment containing 900/90 μatm of $\text{H}_2/\text{H}_2\text{O}$ in helium. No adhesion was observed when the $\text{H}_2/\text{H}_2\text{O}$ ratio was increased to 100 at the same absolute level of H_2O . Additional tests were conducted using spacers fabricated by the electrodischarge machining (EDM) technique. These spacers had flat or concave contact surfaces with 2 μm AA surface roughness. No adhesion was observed under the same conditions at which pressed spacers showed adhesion. In addition, adhesion has not been observed with Inconel 718, Inconel 625, or Hastelloy spacers against type 316 SS smooth rods.

During this quarterly period, preparations were made for initiating the reproducibility and long dwell time tests, which required making two decisions concerning spacer configuration and surface: the first is whether to use a flat or concave surface, and the second is the surface roughness range.

A statistical analysis of the test results for flat and concave contact surface EDM spacers was performed and the following are the conclusions:

1. At 90% confidence level the average coefficient of friction of the concave surface spacer and smooth rod combination is significantly less than the average coefficient of friction of flat surface spacer and smooth rod combination;
2. At 90% confidence level the true difference in the average coefficient of friction can be as little as 0.02 and as much as 0.35;
3. At 60% confidence level the average wear of the concave surface spacer and smooth rod combination is significantly less than the average wear of the flat surface spacer and smooth rod combination;
4. At 60% confidence level the true difference in the average wear can be as little as 2.25 μm and as much as 14.6 μm .

In summary, the concave surface results in less friction and less wear. However, a theoretical layout showed that the fuel rod always tends to bear on the corner of a concave spacer. It was therefore decided that the conservative approach for the reproducibility test was to use the flat configuration, which makes it possible to change the design later to a concave surface if desired.

Tests were performed to provide data for selecting a surface roughness range for EDM spacers for the reproducibility tests. The surface roughness for EDM machine parts is proportional to the rate of machining, and there is a tendency for manufacturers to produce parts with very rough surfaces. The results of these tests for surface roughnesses of 1.3 μm AA to 3.3 μm AA and flat contact surfaces are given in Table 2.5. Conclusions from these tests and previous tests are summarized below.

1. Previous test results indicated high wear rates and friction at the extreme ranges of surface roughness, e.g. low values of surface roughness (less than 0.75 μm AA) and high values (greater than 7.5 μm AA).
2. The present results of Table 2.5 indicate that a spacer surface roughness of 1.4 μm AA to 3.2 μm AA is acceptable. The EDM surface finish specification falls into the number 24 to number 30 range. This surface finish range was selected for all rod-spacer reproducibility tests.
3. Very low or negligible wear was observed in the short stroke tests as shown in Table 2.5. After careful inspection of the test rig it was decided that this might be related to the flat contact surface of the spacer. Previous results with concave surfaces had shown considerable wear.
4. The long stroke test results in Table 2.5 show a mean value of friction of 0.62 with a standard deviation (σ) of 0.12. The mean value of wear is 35 μm with a σ of 8 μm .

Table 2.5
SURFACE ROUGHNESS/FRICTION TEST RESULTS^a

Stroke (mm)	Surface Roughness of Spacer ($\mu\text{m AA}$)	Coefficient of Friction		Wear (Max) (μm)
		Peak	Average	
3.8	3.25	0.5	0.5	37
	3.25	0.6	0.4	25
	2.50	0.7	0.6	50
	2.00	--	--	30
	1.75	0.6	0.6	40
	1.37	0.5	0.5	30
	1.37	0.8	0.7	30
0.76	3.25	0.4	0.4	negligible
	3.25	0.5	0.5	negligible
	2.50	--	--	5
	2.0	0.8	0.4	negligible
	1.75	0.6	0.6	10
	1.37	0.5	0.5	5
	1.37	0.4	0.4	15

^aOther Test Parameters: Temperature - 525°C, $\text{H}_2/\text{H}_2\text{O}$ - 3000/850 μatm , smooth rods and EDM spacers with flat contact surfaces, both of 20% CW type 316 SS.

5. Similarly, the mean value of friction in the short stroke test is 0.53 with a σ of 0.15. The mean wear is 5 μm with a σ of 6 μm .

During this reporting period, four reproducibility tests have been conducted. Reports will be made after additional tests are completed. Long dwell time tests using the third test rig are underway.

2.3.2. Duct Strain Measurements

Other mechanical testing included an investigation of experimental stress analysis on small scale hexagonal ducts for correlating analytical structural analyses using water as pressurizing medium at room temperature, with strain gages and dial indicators for measuring wall deflections.

2.4. CORE TEMPERATURE MONITORING

The fuel element assembly conceptual design layout has been completed, and the preliminary system requirements used as the design basis for the core temperature monitoring system are as follows:

1. The reference design core monitoring device will be chromel/alumel thermocouples.
2. Each element will have a single thermocouple, which is replaceable, inserted in the central fuel rod position.
3. The thermocouple probe will be designed and calibrated to measure mixed element outlet coolant temperature.
4. The thermocouple data or temperature measurements are required for monitoring during reactor startup after each refueling only.
5. The thermocouples will not be part of the automatic safety circuit.

6. A thermocouple connector to the leads in-core is not required for the fuel assembly, and replacement will be accomplished by retracting the thermocouple from above the grid plate. A thermocouple connector for out-of-core connection to leads will be required.

The design requirements for the blanket element assembly are not firm because of changes being made to the conceptual design. However, they will probably be the same as for the fuel assembly.

The design requirements for the control assembly may differ since the central position is not feasible for the thermal probe because of the control rod. The conceptual design of the control element has not been completed.

2.5. HEAT TRANSFER AND FLUID FLOW TESTING

This subtask includes the measurement of the pressure drop across the fuel element assembly inlet nozzle. The proposed experimental technique is to fabricate a full-sized flow model of plastic using air as the test fluid in a similitude test. Since the model dimensions are the same as the design, the similitude requirements are simply to match the Reynolds numbers of air at test conditions with those of helium coolant at GCFR design operating conditions.

	<u>Air</u>	<u>Helium</u>
Temperature	22°C	322°C
Pressure	0.1 MPa	9 MPa
Density	1.2 kg/m ³	7.1 kg/m ³
Viscosity	1.82 x 10 ⁻⁵ N·s/m ²	3.22 x 10 ⁻⁵ N·s/m ²

Simulation for the above conditions can be obtained by matching the Reynolds numbers of air and helium. Since the geometric parameters are equivalent for the air test model and the design, the ratio of air flow to helium flow is equal to the ratio of the viscosity, or $\omega_{\text{air}} = 0.565 \omega_{\text{he}}$. The air blower horsepower requirement is calculated using a maximum helium coolant flow in a GCFR element of 7.70 kg/sec, so the required air flow is 4.36 kg/sec, and the expected maximum pressure drop in the inlet nozzle with grid shielding is 0.036 MPa. Thus, the air blower output power requirement is 130 kW or 175 hp.

Inquiries were made of several experimental facilities that might have blower capabilities that would fulfill the above requirements. These facilities included University of California at San Diego, Oak Ridge Holifield National Laboratory, Convair, and General Atomic HTGR experimental engineering. It was concluded that none of the facilities could fulfill the testing requirements except HTGR experimental engineering. Thus, it is planned to design the test model for connection to the existing blowers at experimental engineering.

REFERENCE

1. "Gas Cooled Fast Breeder Reactor Quarterly Progress Report for the Period May 1, 1975 through July 31, 1975," USERDA, Report GA-A13565, General Atomic, October 1975.

III. PRESSURE-EQUALIZATION SYSTEM FOR FUEL (189a No. SU006)

3.1. FUEL-ELEMENT AND VENT-CONNECTION SEALS

In the GCFR reference design, the fuel-, control-, and blanket-elements and their vent connections are sealed to the grid plate by clamping the conical surfaces of the elements to the matching surfaces in the grid plate with a force sufficient to effect a seal and to support the elements, which are cantilevered from the grid plate. These element seals must function at the coolant pressure difference between the reactor core inlet and exit plenums. The effectiveness of the seals over the life of the core is uncertain, not only because each element may be rotated or relocated several times over its useful life, but also because the seals must be effective in a high-purity, high-temperature helium environment while subject to mechanical, vibrational, and thermal effects. Most of the uncertainties are expected to be resolved in a two-part program: (1) a materials screening test program for study of static adhesion of simulated fuel-element and grid-plate parts clamped together, and (2) leakage tests of fuel-element and vent-connection seals to the grid plate. Current progress in these activities is given below.

3.1.1. Static Adhesion Tests

The element-seal test program includes a simulation test of small scale samples of conical seal parts clamped together for a 3000 hour test in a hot helium environment. The test is a near simulation of conditions of a full scale fuel element clamped to a grid plate. The tests of conical samples made of type 316 stainless steel were completed and reported earlier. Briefly, the results were that some adhesion and material transfer usually occurred for clamping loads typical of a GCFR reactor design.

Because this result was not unexpected, the program plan calls for testing alternative candidate materials. The materials combinations that will be tested are given below.

<u>Fuel Element Cone</u>		<u>Grid Plate Cone</u>
1. Inconel 718 45° angle	vs.	SS type 304 45° angle
2. Inconel 718 30° angle	vs.	SS type 304 30° angle
3. Inconel 718 45° angle	vs.	SS type 316 45° angle
4. Inconel 718 30° angle	vs.	SS type 316 30° angle
5. SS type 316 45° angle	vs.	SS type 304 45° angle
6. SS type 316 30° angle	vs.	SS type 304 30° angle

Test couples have been machined and are now being inspected for cone angle tolerance and concentricity.

3.1.2. Fuel Element Seal Leakage Tests

The major phase of the GCFR element seal development is the leakage test using full-size element and grid sealing parts at reactor operating temperatures. The environment is helium from a commercial cylinder at pressures from 69 to 414 kPa (10 to 60 psi). The conical seal test parts were clamped together at nominal axial net loads of 8.9, 13.3 and 17.8 kN (2000, 3000 and 4000 lb).

The test variables evaluated were surface finish, clamping load, pressure differential across the seal, and cone angle mismatch to determine the effects on seal leakage.

Integral with the measurement of leakage across the fuel element seal, the leakage of the pressure equalization system (PES) vent connection was evaluated. Inleakage around the vent connection was measured as a function of the pressure drop resulting from sweep-gas flow through the suction hole into the PES monitor lines. The other variables in the tests were clamping load, core ΔP , surface finish, and cone angle mismatch between the grid and element.

3.1.2.1. Conical Element Seal Testing. The leakage tests of the GCFR reference design conical element seal and PES vent connection were completed. The final test included a series of side loadings on the simulated fuel element structure. The conical seal parts were machined by grinding the cone angles to a mismatch of less than 2 minutes. The leakages at room temperature were too low to measure for both element and vent connection leakage. That is, the element seal leakage (core bypass) was less than 0.24 ml/sec (0.03 ft³/hr) compared to the specification limit of 1.57 l/sec (200 ft³/hr), and the vent connection leakage at $\Delta P = 13.8$ kPa (2 psi) was less than 0.63 ml/sec (0.08 ft³/hr) on one side and zero on the opposite side, compared to a specification limit of 3.1 ml/sec (0.4 ft³/hr). When side loading was applied with steps of 889 N (200 lb), the element seal leakage (core bypass) and vent connection leakage became excessive (an order of magnitude greater than the specification) at a side load of 2444 N (550 lb). This load represents a turning moment on the element of about 1335 N·m (1000 ft·lb) since the lever arm is slightly greater than 0.53 m (21 in.). So at the end of a 2.74 m (9 ft) long element, a force of 490 N (110 lb) would cause the same bending moment. When the load was removed, the element and vent connection leakage rates returned to the low preload magnitudes. The element was then rotated, and the element leakage was again too low to measure; and the vent connection leakage was again zero on one side and low on the other side. The element leakage remained low until a side load of 2667 N (600 lb) was applied, when it increased to 0.13 l/sec (16 ft³/hr) at a clamp load of 17.8 kN. This side load represents a turning moment of 1971 N·m (1455 ft·lb) or a force of 719 N (160 lb) at the end of a 2.74 m (9 ft) long element. When the clamp load was reduced to 13.3 kN and then 8.9 kN,

the seal leakages increased to 3 ℓ /sec (380 ft^3/hr) and 7.7 ℓ /sec (940 ft^3/hr). When the side load was then increased to 3556 N (800 lb), the leakages went off the scale on the flow meters. When the load was removed, the seal leakages became much lower, but did not return to their pretest values. The element leakage was about 0.16 ℓ /sec (20 ft^3/hr), and the vent connection was again zero on one side and about 6.3 ml/sec (0.8 ft^3/hr) on the other side at $\Delta P = 13.8$ kPa. The parts were then separated, reseated, and clamped together, and the leakages remained the same. The parts were then taken apart and the surfaces were examined. There were shallow linear indentations where the cones were in line contact and at the hex corners where contact occurred while turning under the applied side loads. The higher leakages after side load testing apparently resulted from these indentations.

3.1.2.2. Summary of Conical Seal Testing. The completion of the preliminary tests on the reference conical seals is a logical place for a summary and the conclusions of this seal design performance. The initial planning included tests to investigate the effect of the following parameters on element and PES vent connection seal performance:

1. Material surface finish (i.e., roughness values) of 0.2 mm (8 $\mu\text{in. AA}$), 0.4 mm (16 $\mu\text{in. AA}$), and 0.8 mm (32 $\mu\text{in. AA}$),
2. Cone angle mismatch (i.e., simulated angle tolerances) of ± 2 minutes, ± 4 minutes, and ± 6 minutes,
3. Net clamping forces of 8880 N (2000 lb), 13,320 (3000 lb), and 17,760 N (4000 lb),
4. Core pressure drops across seal parts, from 68.9 kPa (10 psi) to 345 kPa (50 psi),
5. PES suction hole pressure drop in the range of 6.9 kPa (1 psi) to 27.6 kPa (4 psi) (vent connection seal only),

6. Element and grid cone angles of 30° , which is the reference design value.

In parallel with the seal test, a series of static adhesion tests was conducted with small scale samples of the conical seal parts clamped together in a hot helium environment simulating the GCFR environment. The test variables include items 1 and 3 above, for the seal leakage test, with cone angles of 30° and 45° .

The seal leakage tests were full scale with a nearly exact simulation of the reference design conical seals. The simulation of the alignment of cones was not included because this is a design interface with the element latching mechanism design, which was incomplete. The test was designed to test the sealing capability of the clamped metal cone surfaces without interface effects. This was done by making the parts self-aligning by using an articulated joint between the test part fuel-element and the tension rod that clamped it to the grid plate test part. Usually this clamping method was satisfactory. However, whenever the materials galled, they were difficult to align and seat to form a contact seal. This was particularly true at elevated temperatures. A dry, high temperature molybdenum disulfide lubricant powder was tried on one set of parts but provided no improvement. Because of the tendency of parts made from type 300 stainless steels to gall when in contact with one another, this seal design will probably require dissimilar materials at the element and grid plate cone surfaces.

The leakage test series was conducted in a sequence that permitted some judgement during testing and thus might preclude further parametric testing. The surface finish values were selected by assuming that grinding of the cone angle would be required for accuracy. The as-ground surface is $0.4\text{ }\mu\text{m AA}$ to $0.8\text{ }\mu\text{m AA}$. The lowest roughness of $0.2\text{ }\mu\text{m AA}$ was tested first and then the $0.8\text{ }\mu\text{m AA}$ roughness was tested. The results showed that there was insignificant difference in seal leakage between the surface roughness of $0.2\text{ }\mu\text{m AA}$ and $0.8\text{ }\mu\text{m AA}$. Also, with the $0.2\text{ }\mu\text{m AA}$,

the parts tended to stick together at room temperature; and the adherence was enhanced at a temperature of 300°C and the highest clamping force of 17,760 N. This result agreed with the results from the static adhesion tests.

The test for cone angle tolerance effect was initiated with ± 4 minutes tolerance. Taking the extremes of $30^\circ + 4'$ for the grid cone angle and $29^\circ + 56'$ for the element cone angle resulted in an average cone angle mismatch of 8 minutes. Two tests were conducted with this mismatch, and three tests with a mismatch of 4 minutes, with the following results:

1. When the conical parts were properly aligned and seated, an effective seal against helium bypass flow resulted.
2. The vent connection leakage always exceeded the allowable specification of 3.15 ml STP/sec ($0.4 \text{ ft}^3/\text{hr}$) with the large angle mismatch of 8 minutes.
3. The vent connection bypass leakage was always below the specification of 3.15 ml STP/sec with a 4 minute mismatch except when the parts became cocked due to galling and/or misalignment of the cones.
4. With the larger cone angle mismatch, the cones become cocked more often and galling occurred, which resulted in excessive leakage.

Two major conclusions were then made concerning surface finish and cone angle mismatch. First, that a surface roughness of $0.4 \mu\text{m AA}$ to $0.8 \mu\text{m AA}$ is entirely adequate for the seal design. Second, that a nominal cone angle mismatch of 4 minutes or less is required for acceptable sealing of the PES vent connection. Since the machining method for the cones is done on a precision grinder, the cone can be machined to within 2 minutes over or under the nominal 30° angle. Thus, the subsequent

test parts were machined to an angle of $30^{\circ} \begin{smallmatrix} +2' \\ -0' \end{smallmatrix}$ for the grid and $30^{\circ} \begin{smallmatrix} +0' \\ -2' \end{smallmatrix}$ for the element. This has the added advantage of better alignment of the parts. Also the as-ground parts have a surface roughness of 0.4 μm AA to 0.8 μm AA. The static adhesion tests showed that this surface is less likely to result in adherence.

Other conclusions from the parametric test results include:

1. When a seal is seated, clamping loads above 4440 N (1000 lb) have little effect on the seal leakages.
2. Similarly, the core pressure drop up to 375 kPa (50 psi) has no effect on leakage if the cones are properly seated.
3. The vent connection leakage is usually linear with the PES system pressure drop if the seal parts are properly aligned and seated.

Finally, therefore, making a metal-to-metal conical clamped seal at 325°C within design specified leakage is initially achievable. However, it is doubtful that both seal parts made of 300 series stainless steel will be adequate after many cycles of being clamped, rotated, and reclamped. This is because of the material deformation and transfer between surfaces observed in short periods of time in the high temperature seal leakage tests and the static adhesion tests. Static adhesion testing of alternative materials combinations is in preparation.

3.1.3. Piston Ring Element Seals

An alternative element seal design is being developed in parallel with the reference design conical seal. The alternative design uses piston ring seals. Two designs differing only in details are being developed: one with Kraftwerk Union and another with Koppers Company. Test equipment will be modified to test the piston ring seals.

3.1.4. Vent Port Seal Development

A valve is being developed to combine the functions of a PES vent connection between the grid and element and the sealing of the element vent port when removed from the reactor. Two fabrication practice models have been made. One of the models was successfully welded, but has not yet been leak checked. Difficulty was experienced with the welding of the bellows end on the other assembly.

3.2. ANALYSIS, MODELS AND CODE DEVELOPMENT

The transient flow network analysis of the GCFR pressure equalization system (PES) was resumed in this quarter after having been postponed due to budget reduction a year ago. Efforts focused on reviewing the survey of available transient flow codes⁽¹⁾ completed in FY-75, the earlier steady-state analysis, reviewing candidate transient analysis codes, and developing an initial solution technique. The steady-state network code FLAC⁽²⁾ was reviewed. This was the code that had been used for the PES steady-state network analysis.

Initially, it was considered necessary to develop a single model that simultaneously incorporated simulations of the GCFR coolant system, the helium purification system unit (HPS) in the PES, the PES monitorline network, and the fuel and blanket rods themselves in a manner that would have been a direct extension of the earlier steady-state analysis. In the course of the initial study, it became apparent that rather than use a single model to simulate the entire system, it is more appropriate to break the system into three subsystems whose dynamic characteristics are such that an individual subsystem may be analyzed, partially or completely, separately from the remaining subsystems. The three subsystems are: (1) the main coolant system, (2) the PES-HPS, and (3) the PES monitor line network together with typical elements or groups of elements.

Transient dynamic models of the main GCFR coolant system have already been developed, and discussions were held with HTGR personnel regarding

the transient analysis of the HTGR HPS. The method that was to have been employed in that analysis was to obtain the solutions of the basic differential equations using FORTRAN together with an integration package. As a result of these discussions, two computer codes, FAT and RATSAM, were obtained for further study. Each code was used to run a small sample problem to examine its suitability for the GCFR PES transient analysis.

RATSAM is a GA code currently being developed and used to study the transient flow behavior of the main cooling system of the HTGR during depressurization accidents, although it is expected to have the capability of solving general transient flow problems as well. However, considerable modification of that portion of the code used for general problems was necessary to get the code to accept the small trial problem.

The FAT code is a transient version of FLAC and thus is a general purpose code that solves for the transient flows in a network consisting of volumes interconnected by pipes. Neither FAT nor RATSAM has provisions to handle boundary conditions. As a result, both codes are limited to the analysis of closed-loop systems.

After reviewing the various transient flow analysis codes available at GA, it was decided to attempt to develop a simple program for the analysis of systems 2 and 3 using the SYSL simulation language. SYSL is General Atomic's System Simulation Language, a computer code used for simulating the behavior of physical systems on a digital computer, similar to the IBM code CSMP⁽³⁾.

Starting from first principles, the continuity, momentum and energy equations were coded utilizing SYSL's MACRO capability. A lumped sub-volume approach was used to analyze a model, representative of the type of models that will be used to simulate the HPS. The model consisted of two volumes connected by a pipe. Each volume was also connected by pipe

to large plenums, the pressures and temperatures of which could be specified as functions of time. The particular transient considered was a depressurization of the two large plenums from 100% pressure to 20% in 20 seconds. The temperature of the large plenums was assumed to remain constant. Starting from arbitrary initial conditions and the given boundary conditions at time zero, the SYSL code achieved an initial steady state. Transient operation of the code was then begun. Output consisted of the pressure and temperature of the gas within each of the two volumes, and the mass flow rates through each of the three pipes.

Although the results to date have not been entirely satisfactory because some minor inconsistencies remain to be resolved, it appears that this method will work; and further it appears that, of all the solution techniques examined so far, this one will prove to be the most desirable.

Thus, the solution technique that has been selected is as follows: (1) use of one or more of the existing system transient analysis codes to obtain the GCFR coolant system dynamic behavior, and (2) use of the SYSL simulation language first to model the helium purification system, and then to model the fuel assemblies connected to the PES network. These will initially comprise three separate models, although the latter two models may be combined into one with relatively little effort.

3.3. PLATEOUT AND PLUGGING

Efforts in this reporting period were expended in the calibration of analytical equipment for measurement and characterization of the impurities that will occur in the helium carrier gas transporting fission products through the GCFR-PES and the plateout and plugging loop. These measurements will be used to determine and study the important effects leading to or controlling plateout. Oxygen and trace gas analyzers were calibrated and tested. Work was also in progress on high pressure differential pressure transducers and the helium circulator.

3.3.1. Oxygen Analyzer

An oxygen analyzer with a zirconia cell was returned from the manufacturer after being fitted with a reference gas sleeve. At high levels of water vapor the instrument response was found to be quite linear with $\log (H_2/H_2O)$, but below 4.5 Pa (45 μ atm), the cell EMF deviates from linear response.

3.3.2. Trace Gas Analyzer

The Varian trace gas analyzer (model 2731) was calibrated for H_2 using an exponential dilution flask technique. Calibration runs were made over the range from 300 to 0.6 ppm using a detector voltage of 400 V. The concentration range, from $\sim$ 180 to 1200 ppm was calibrated with a voltage of 300 V. Since the detector response is not linear with concentration, a series of calibration curves was developed. The analyzer was then tested for H_2 content in helium against commercial gas mixtures with the following results:

<u>Supplied Analysis (ppm)</u>	<u>Trace Gas Analysis (ppm)</u>
48	48.0 ± 1.5
10	8.9 ± 0.6
43	39.5 ± 1.6
495	501.1 ± 1

A tritium-in-helium source gas (nominal 1000 ppm) measured 992 ppm and "hydrogen free" tritium measured (estimated) $\sim$ 15 ppb based on extrapolation from calibration data at 600 ppb.

3.3.3. High Pressure Differential Pressure Transducers

Five differential pressure transducers were calibrated at atmospheric pressure using water or mercury manometers depending on the pressure

difference range. Previous tests have shown that calibration at atmospheric pressure is retained at elevated pressures.

3.3.4. High Pressure Helium Circulators

One of two helium circulators to be used for circulating helium in the "plateout and plugging" loop to be assembled to study the transport and plateout of cesium has been mounted in its pressure vessel and pressurized with helium to 1.03 MPa (150 psig). Satisfactory operation of the pump was attained. The principal purpose of the test was to insure that there were no pressure sensitive or structurally deficient parts. None were found.

A duplicate of the circulator, tested as described above, was tested in air to develop its characteristic curve (shown in Fig. 3.1) for use in selecting the operating point for the circulators in the design of the loop.

3.3.5. Cesium-Fuel/Blanket Interaction

The program being conducted at ANL in support of the GCFR on cesium-uranium reaction thermodynamics and kinetics is progressing according to schedule. A review of the work was conducted during a visit to ANL in August.

3.4. PES MANIFOLD FABRICATION

Manifold fabrication development during this period consisted of:

1. Evaluation of cast one-third manifold segments by nondestructive methods,
 - a. X-radiography,
 - b. Ultrasonic.

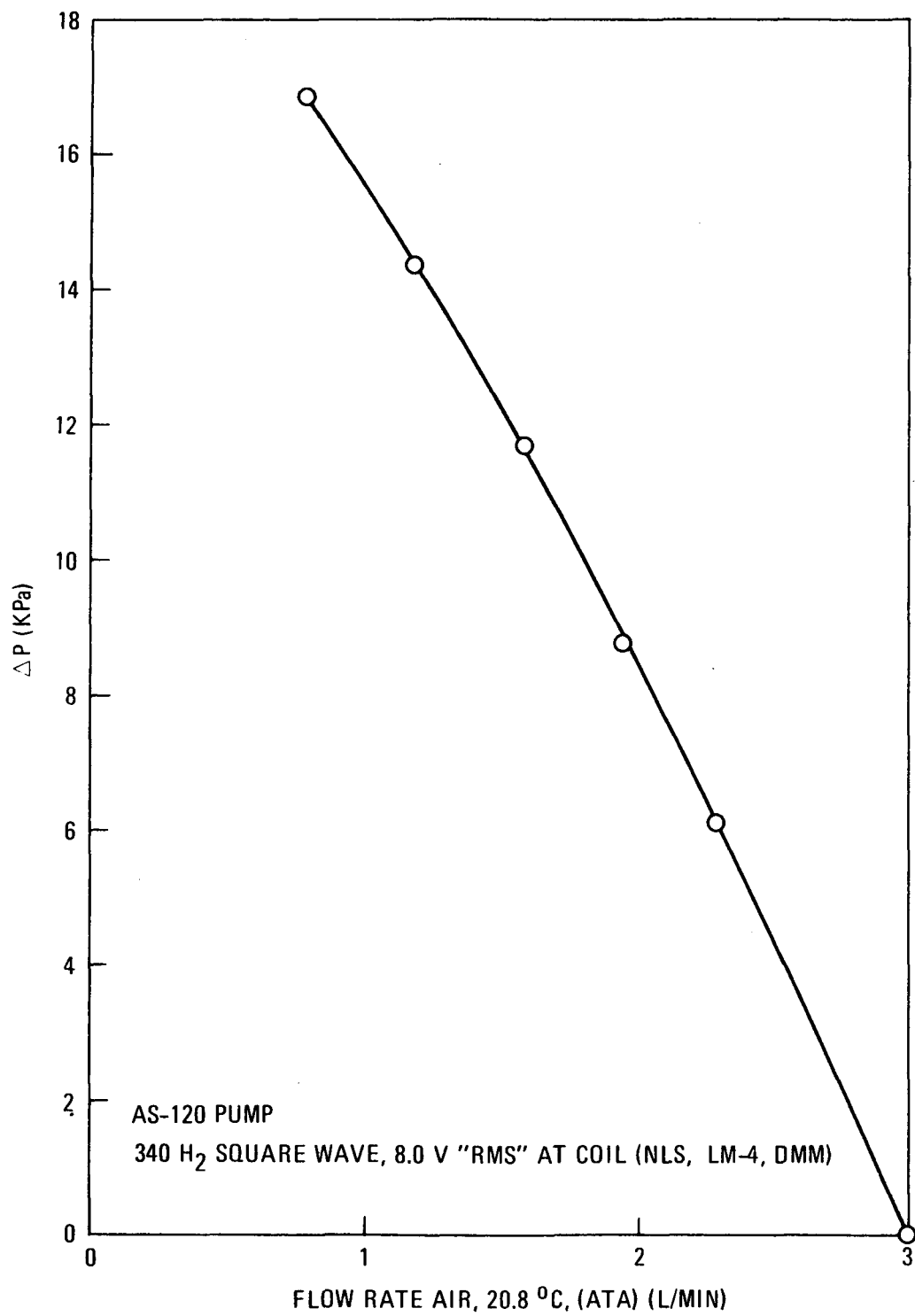


Fig. 3.1 Characteristic curve of a helium circulator for the plateout and plugging test loop

2. Evaluation of three methods of drilling fission gas passage holes into one-third manifold segments by three different vendors,
 - a. Electrochemical machining (ECM) by Metem,
 - b. Electrical discharge machining (EDM) by L. C. Miller and Associates,
 - c. Gun drilling by Deep Hole Specialists.
3. Microprobe analysis of one of the cast one-third manifold segments,
4. Preliminary evaluation of three full size investment cast manifolds,
5. Electron beam welding trials on manifold parts in which a cover plate with milled fission gas passages is welded to the manifold structure,
6. Leak testing of double rod to manifold seals that would allow making the connection between the rod and manifold from the top of the manifold.

3.4.1. Non-destructive Examination

Two manifold segments were examined by ultrasonic techniques with marginal results. This is due to the large grain size in the castings. A report of the ultrasonic examination, x-radiograph examination, and a review of the drawing is being prepared.

3.4.2. Hole Drilling

Drilling of 1.27 mm (0.050 in.) diameter fission gas passages (by ECM) in a one-third segment of a manifold structure was completed. The holes had an excellent finish and were very straight. Two of the thirteen

holes [10 mm (3.5 in.) long] broke through the side of the webs. The breakthrough is attributed to miscalculations of the positions of the webs as well as the manifold segment not having been fabricated exactly to the nominal dimensions on the drawing.

The one-third investment cast manifold structure segment has been evaluated and returned to the Metem Corp. for installation of the threaded rod connection holes. The locations of the holes in the segment were essentially the same as reported by the manufacturer. The fission gas passage holes were sealed at the perimeter of the manifold and at the two locations of hole breakthrough. Leak testing showed both the welds and the manifold to be helium leak tight even though the fission gas passage holes were within a few mils of the surface in several locations.

L. C. Miller and Associates have completed drilling of all fission gas passages (by EDM) on a one-third segment of a manifold structure. The part has not yet been received at GA.

Deep Hole Specialists have reported difficulties in runout of the pilot drill used for gundrilling. Breakthroughs have been repaired by welding and the holes are being redrilled. They expect to complete the gundrilling within a week of this writing.

3.4.3. Microprobe Analysis

Sections of the 316 SS manifold structure castings from Arwood were analyzed by Kraftwerk Union (KWU)* by means of an electron microprobe. The analysis indicated that the constituent elements were segregated at the grain boundaries, that is, chromium, molybdenum, and silicon were higher than in the matrix while nickel and iron were lower than the matrix. Since such segregation had not been noted during microprobe analysis of the same casting (but from a different web here at GA), samples from the same casting were resubmitted to specifically analyze for grain boundary segregation. The new analysis of the grain boundaries showed chromium

*This work is not funded by ERDA.

and iron slightly below the matrix and nickel well below the matrix levels. Molybdenum was just detectable in the grain boundary, and silicon was just readily detectable in both the matrix and grain boundaries. Information on the comparative sensitivities of the KWU and GA microprobe equipment and exchange of samples for cross-analysis will be sought to resolve the differences in results.

3.4.4. Complete Manifold Castings

The three full manifold structures of 316 SS, investment cast by Arwood, have been received and are undergoing inspection. The vendor's analysis shows the carbon and silicon contents of the castings to be well below specified limits. (KWU had expressed concern that SS containing high carbon and silicon would not perform well in the presence of impurities in helium.)

3.4.5. EB Welded Manifolds

Electron beam welding of manifold web sections to cover plates, at Chem-Tronics, has been completed. Preliminary examination by the vendor indicates the parts to be satisfactory. The parts remain to be evaluated at GA.

3.4.6. Fuel-Rod Joints to Manifolds

Initial test results at room temperature indicate that a double, flat bottom seal has a leak rate 10 to 100 times lower than single flat bottom seals previously tested. The surfaces were rechecked to determine if the difference is due to different surface conditions in the double seal test specimens, as compared to the single seal test specimens. No significant differences were found. Two double seals have been tested as fabricated and assembled and then after breaking and remaking three times; the leak rates were very similar in every instance.

The reduction in leak rate is now attributed to the fact that there is a rather generous tolerance between the mating parts, and this allows better self-alignment and fit-up of sealing surfaces to occur in the double seal than in the single seal design.

3.5. PES PROGRAM PLANNING

No efforts were expended during this quarter.

REFERENCES

1. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period August 1, 1974 through October 31, 1974," USAEC Report GA-A13238, General Atomic, December 1974.
2. Malek, G. J., "Development of the Flow Analysis Code FLAC," USAEC Report GA-9482, Gulf General Atomic, June 1969.
3. "IBM Application Description Manual, System/360 Continuous System Modeling Program (CSMP)," International Business Machines, Inc. Form GH20-0240.

IV. CORE ASSEMBLIES STRUCTURAL-THERMAL-FLOW TESTS (189a No. SU006)

A series of out-of-pile simulation tests are to be performed to demonstrate the ability of the GCFR fuel-, control-, and blanket-assembly designs to meet design goals and to verify predictions of analytical models that describe design operation and accident behavior. The test emphasis will be on obtaining thermal-structural data for steady-state, transient, and margin conditions using electrically heated rod bundles in a dynamic helium loop. The requirements include testing in the range of cladding melting and the consequences of local initiation of melting.

The "Program Plan for the GCFR Core Flow Test Loop"⁽¹⁾ contains the requirements for the test program to be conducted in the core flow test loop (CFTL) that will be constructed and operated by Oak Ridge National Laboratory (ORNL). Work during this quarterly period has included: (1) an initial CFTL PERT* layout, a framework for further detailed planning, (2) analysis in selected areas, (3) a draft of the test specification format, (4) a proposed change in the number and size of CFTL test bundles to reflect changes in GCFR designs, and (5) preparation for and participation in a coordination meeting with ORNL.

4.1. PROGRAM PLANNING

4.1.1. Program Plan

The "Program Plan for GCFR Core Flow Test Loop"⁽¹⁾ is the basic planning document for the program, and it is being supplemented by other specialized documents: a PERT network, test analysis and predictions, test specifications, test bundle designs, and fabrication plans. The initiation of a PERT network for CFTL is discussed below. The draft section on analysis and prediction requirements was approved for inclusion in the CFTL program plan

*The PERT program in use at GA employs PERT, CPM, and cost control techniques and is referred to as the Resource Evaluation and Control System (RECS).

with comments, and a proposed resolution of these comments has been suggested. Following the trial issue of a sample test specification last quarter,⁽²⁾ the comments were reviewed this quarter, and a tentative test specification format was issued for review. An initial schedule for design and fabrication of CFTL test bundles was prepared to determine the latest starting date, and this information will be used in the PERT network. The accumulation of information for the proposed first revision to the program plan is continuing with a target issue date of June 1976.

4.1.2. PERT Network

The GCFR program has developed an overall PERT type schedule of which the CFTL program is a part. The interdependencies and complexities of the CFTL program need the PERT approach to add realism to planning, to coordinate plans at all levels, to pre-evaluate project changes, and to define schedules. An initial skeleton PERT network has been generated including the three principal participants and their major activities as follows:

1. ERDA-DRRD

Program review and approval
Funding

2. GA

Program definition and planning
Test analysis and prediction
Test specifications
Test bundle design
Specialized component fabrication
Data analysis and application

3. ORNL

Test loop design and construction
Test loop operation and data reduction
Test bundle components development
Test bundle fabrication and integration into loop

The definition and interfaces between these activities is complex and is in the process of being clarified with the help of PERT. The critical path for the program involves the loop design and construction, which are being constrained by funding. The second longest path involves test bundle design, fabrication, and testing.

4.2. TEST ANALYSIS AND PREDICTION

4.2.1. Thermocouple Response Time

The response times of thermocouples within heaters and in the helium stream have been suggested as possible sources of errors during transients. In reviewing the factors affecting transient accuracy as compared with steady-state accuracy, it is immediately apparent that additional uncertainty will be introduced by the additional parameters that must be considered in analyzing transient temperature response. These parameters may be expressed in terms of four dimensionless groups:

1. An unaccomplished temperature change =
$$\frac{\text{surrounding media temperature} - \text{T/C temperature}}{\text{surrounding media temperature} - \text{original T/C temperature}},$$
2. A relative time = $\frac{\text{thermal diffusivity} \times \text{time}}{\text{principle dimension squared}},$
3. A radius ratio = $\frac{\text{local position}}{\text{principle dimension}},$
4. A thermal resistance ratio between the media and the thermocouple.

The relationship among these parameters is drawn from McAdams.⁽³⁾

The range of property values composing these parameters is considered for two configurations: sheath thermocouples mounted inside a heater and thermocouples mounted in the gas stream; the specific values are:

Thermal Diffusivity Values

Stainless steel sheath	$5 \frac{\text{mm}^2}{\text{sec}}$
Thermocouple wires	$5 \frac{\text{mm}^2}{\text{sec}}$
Insulation	$1.3 \frac{\text{mm}^2}{\text{sec}}$
Boron nitride	$16 - 5 \frac{\text{mm}^2}{\text{sec}}$

Principle Dimensions

Location	Heater sheath	Helium stream
T/C size, OD, mm	0.7 to 0.2	3 to 1

Resistance Ratio

Heater sheath T/C	0.5 to 2.0
Helium steam T/C	0.3 to 4.0

For a step change in the temperature of the material surrounding the thermocouple, the thermocouple response time is defined for the "unaccomplished temperature change" as the time to decay by $1/e$ (0.368) of the step.⁽³⁾ For the heater sheath thermocouples, the median response time is estimated to be 0.01 seconds; and a slowest response time is estimated to be 0.06 seconds for a particular combination of parameters. This compares with a heater surface response time of 0.5 to 10 seconds depending on the helium flow and associated heat transfer coefficient. The internal sheath thermocouple has a response time of at least a factor of 10 faster than the heater, and therefore the thermocouple can readily follow the transient temperature of the heater without significant error. Similar analysis for the gas thermocouples is expected to yield similar results, particularly when it is realized that the gas temperatures are expected to change at a slower rate than the local heater temperatures. For both the internal heater and the gas stream thermocouples, the relatively fast response time, as compared to the expected transient temperature change of the surrounding

media, will yield small and not very significant values for the "unaccomplished temperature change." Thus any error in this value will also be small. From this review it is concluded that thermal considerations indicate that the transient plus the steady state errors are essentially the same as the steady state errors alone, because the "unaccomplished temperature change" values will be small. Because the possible errors caused by other factors, such as property changes at temperatures above 800°C and electrical interference, may affect both steady state and transient accuracy, these factors are being investigated by ORNL.

4.2.2. Low Flow Measurement Limit

Possible operating conditions, shutdown and refueling, and extreme accident conditions involve postulated conditions where average helium flow through the core assemblies will be in the laminar and transition range, i.e. the Reynold's number approaching 2000 and lower. With significant sub-channel geometry differences between the interior and perimeter of a rod bundle and the buoyant force opposing the normal direction of downward helium flow, uncertainty exists in the prediction of performance for extremely low flow. The performance of CFTL tests is desired to check predictions in this range. ORNL has indicated that flow measurements are more difficult and accuracy is degraded at these flows because of the associated pressure drops in the range of a few millimeters of water. ORNL has stated that the pressure drop measuring limit is approximately 50 mm (2 in.) of water in which the instrumentation is designed for a system pressure of 10 MPa. An initial estimate of a test bundle low-flow measurement limit is illustrated in Fig. 4.1, showing the relationship of Reynold's number versus system pressure for a constant value of bundle pressure drop of 50 mm of water. From the curve it is concluded that operation is measurable in the laminar flow regime with high pressure instrumentation, provided the system pressure is 2 MPa (20 atm) or less.

Other options are available such as performing tests at atmospheric pressure and low flow with special more sensitive pressure instrumentation,

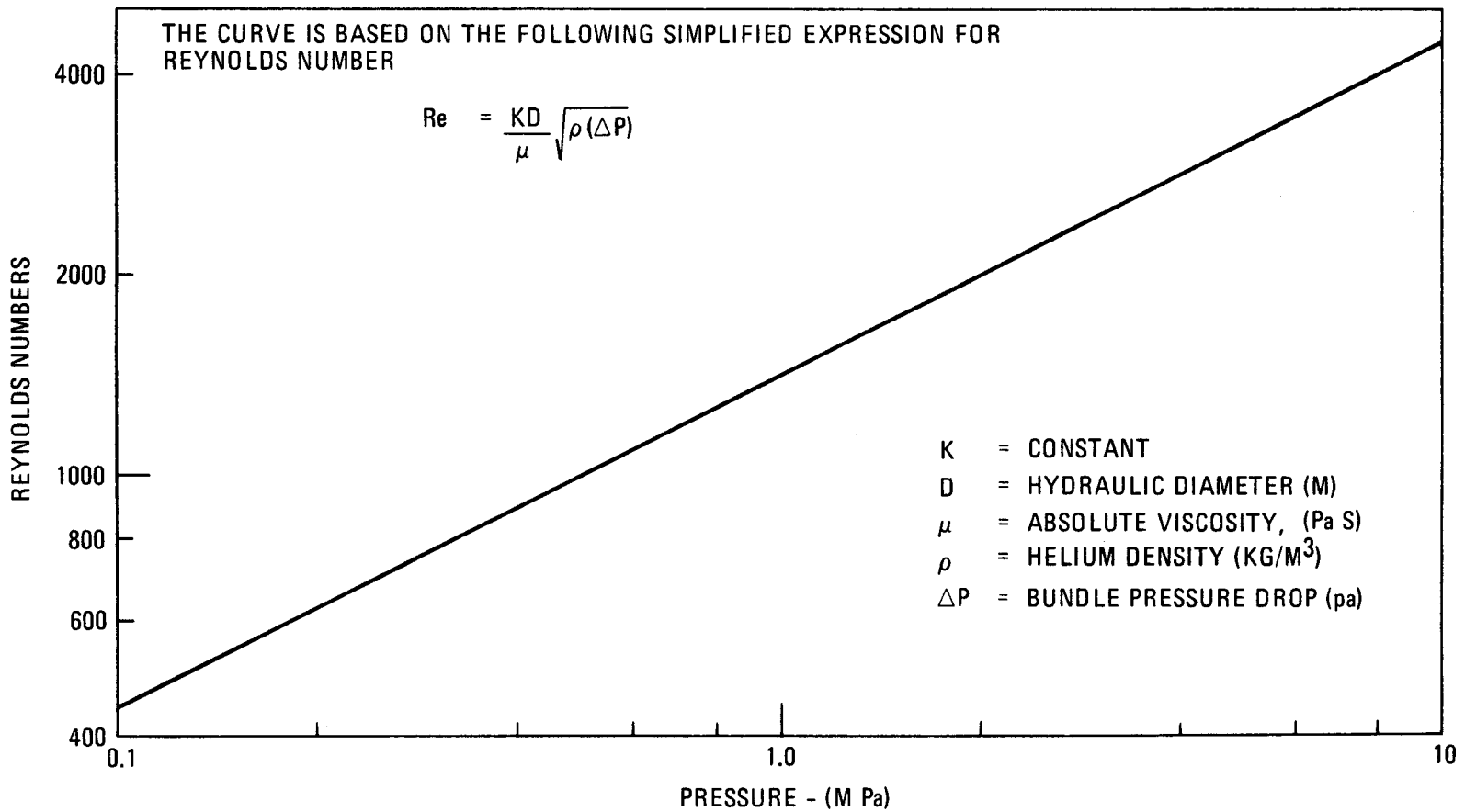


Fig. 4.1 Reynolds Number versus system pressure [average core temperature = 470°C, bundle pressure drop = 498 Pa (2 in. H₂O)]

or relying on heater temperatures alone to indicate flow abnormalities. In addition, these low flow test requirements may cause loop-operational and loop-flow stability problems; these potential problems will be discussed with ORNL.

4.2.3. Core Flow Test Loop Dynamics

The dynamic analysis of the core flow test loop continued during this quarterly period. Efforts focused on (1) developing a new bundle simulation model for either nuclear heated GCFR fuel rods or electrically heated CFTL rods, (2) verifying the performance of this model against the MINGAF code used by the safety analysis branch to compute core temperatures, and (3) rerunning the case of the CFTL simulating the GCFR scram, using the new bundle simulation model. Also, a simplified version of the CFTL dynamic model was developed to facilitate and reduce the cost of doing loop parameter studies, and another program was developed to determine correct steady state heat exchanger air flows.

The new bundle simulation model, developed to take the place of the earlier less detailed one, consists basically of one SYSL MACRO^{*} to compute the temperatures of one axial segment. Typically, six axial segments are used: four for the active core, and one each for the upper and lower blanket. Each axial segment is divided up into six radial regions (four for the fuel or electrical heater, one for the cladding, and one for the helium).

In order to verify the accuracy of this model, a comparison with the GCFR reference transient analysis code MINGAF was made. Parameters in both codes were set to model the 300 MW(e) GCFR core. Two transients were run with the codes: a simulated GCFR scram and a 100% decrease in power, and in both cases agreement was found to be excellent. Core power and flow were varied in a series of linear ramps as shown in Fig. 4.2. This is an approximation of the actual core power and flow expected during a scram of the primary reactivity shutdown system.

^{*}SYSL is GA's System Simulation Language, a digital simulation computer code. A MACRO in SYSL is analagous to a subroutine in FORTRAN.

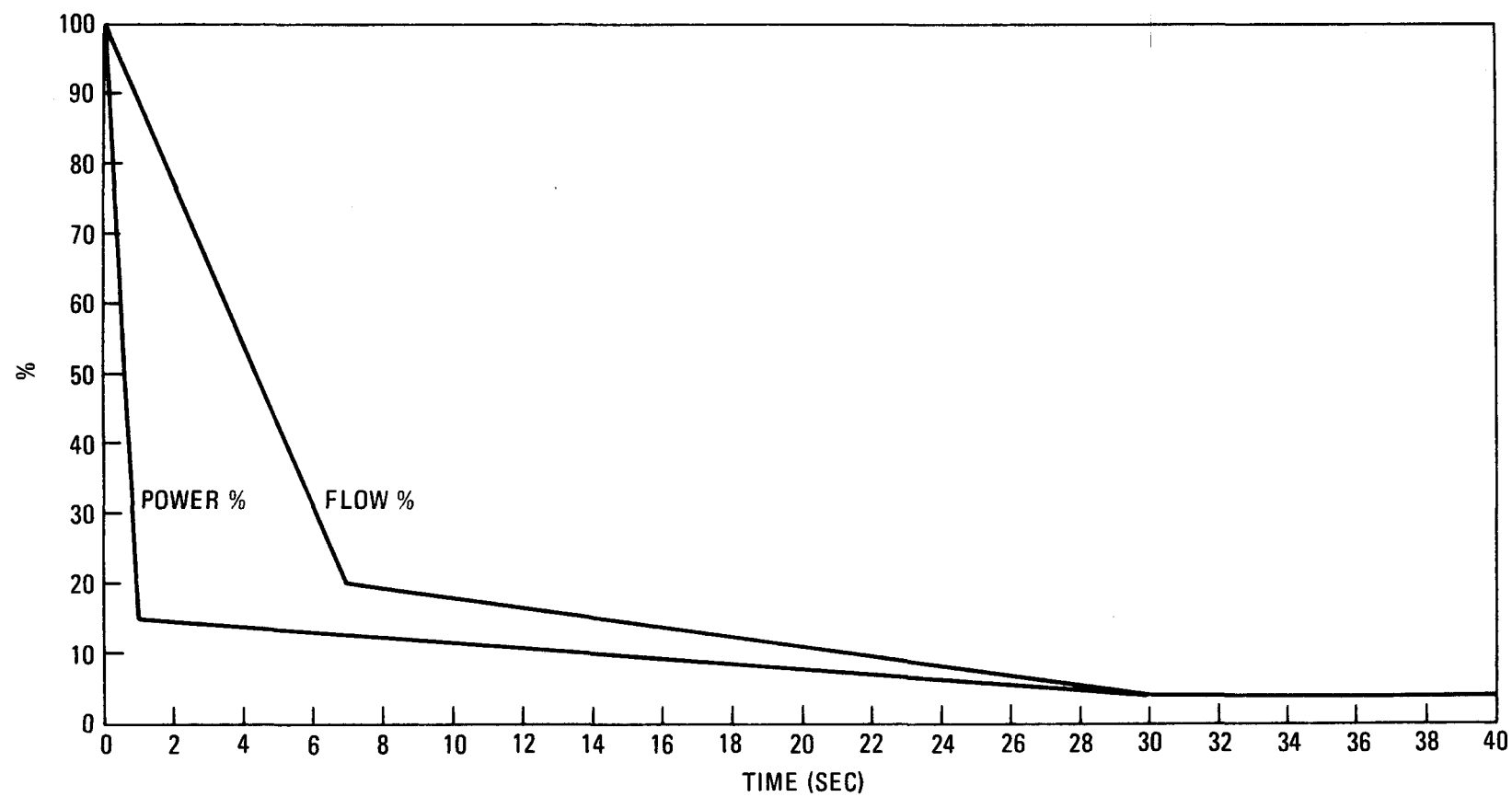


Fig. 4.2 Power and flow values used in comparison of SYSL and MINGAF (simulated GCFR scram)

Average cladding midwall temperatures in degrees centigrade for the smooth section of the active core (first 1/4) and the hottest roughened region (last 1/4) are shown in Figs. 4.3 and 4.4. As can be seen from the graphs, the temperature responses predicted by the two codes are very similar, with only about a 1°C difference at most points.

The new bundle simulation model has been incorporated into the test loop simulation model. As a check on the performance of the loop model with this new bundle simulation model, the case of the CFTL simulating a GCFR scram (described in the previous quarterly report),⁽²⁾ was rerun. Bundle power* and flow are shown in Fig. 4.5, and the loop response is shown in Figs. 4.6, 4.7 and 4.8. The results show general agreement with the earlier calculations, but there is a significant improvement in the accuracy of the cladding temperature transient response, as predicted by the MINGAF code for the GCFR. Notice that although the test vessel outlet temperature drops approximately 100°C in 10 seconds, the circulator inlet temperature varies only by about 4°C, and the test vessel inlet temperature varies only about 1°C. This indicates that no thermal shock is experienced by the circulator, and that the loop is able to maintain prototypic GCFR thermal conditions for this transient.

With the incorporation of the new bundle simulation model, the size of the CFTL simulation model has increased. The loop model now has 46 integrators, 165 input parameters, and over 2100 internal variables. This is a large model. By using an efficient variable step integration method, and by executing from an absolute element (a non-relocatable machine language file), computer run times are held to a minimum. Nevertheless, parameter

*As noted in the previous quarterly, the CFTL will employ electrically heated rods to simulate the GCFR fuel and blanket rods. The heat capacity of these heater rods will be only one tenth of the heat capacity of the prototype rods. To account for this difference, it is planned to modify the power transient supplied to the test section in such a way that the cladding thermal response of the heater rods is made the same as that of a prototype rod. For the purposes of this simulation, the heater rods were given the same thermal characteristics as GCFR fuel rods, so that no modification of the power transient was needed.

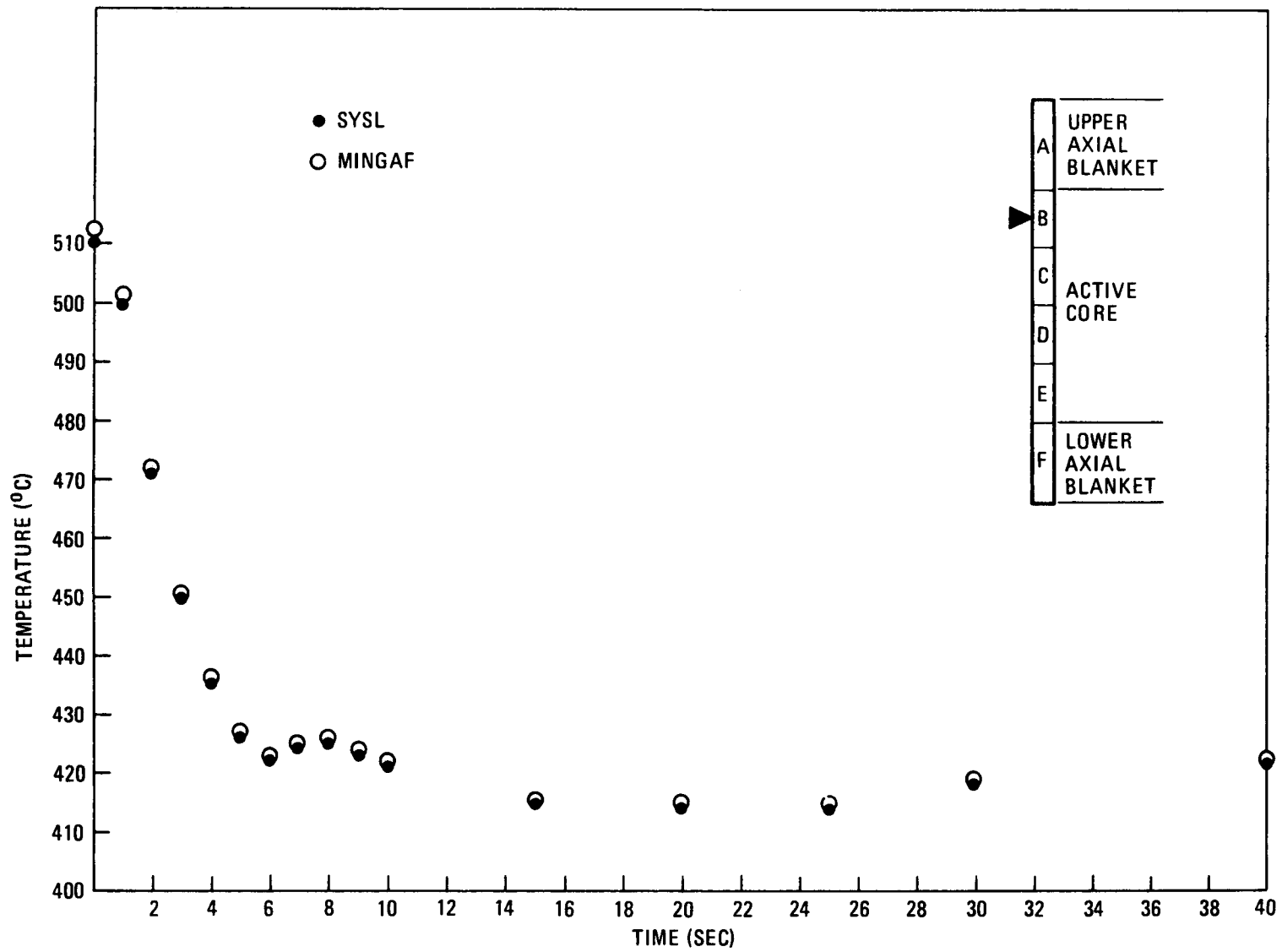


Fig. 4.3 Simulated GCFR gravity scram cladding temperatures - smooth region

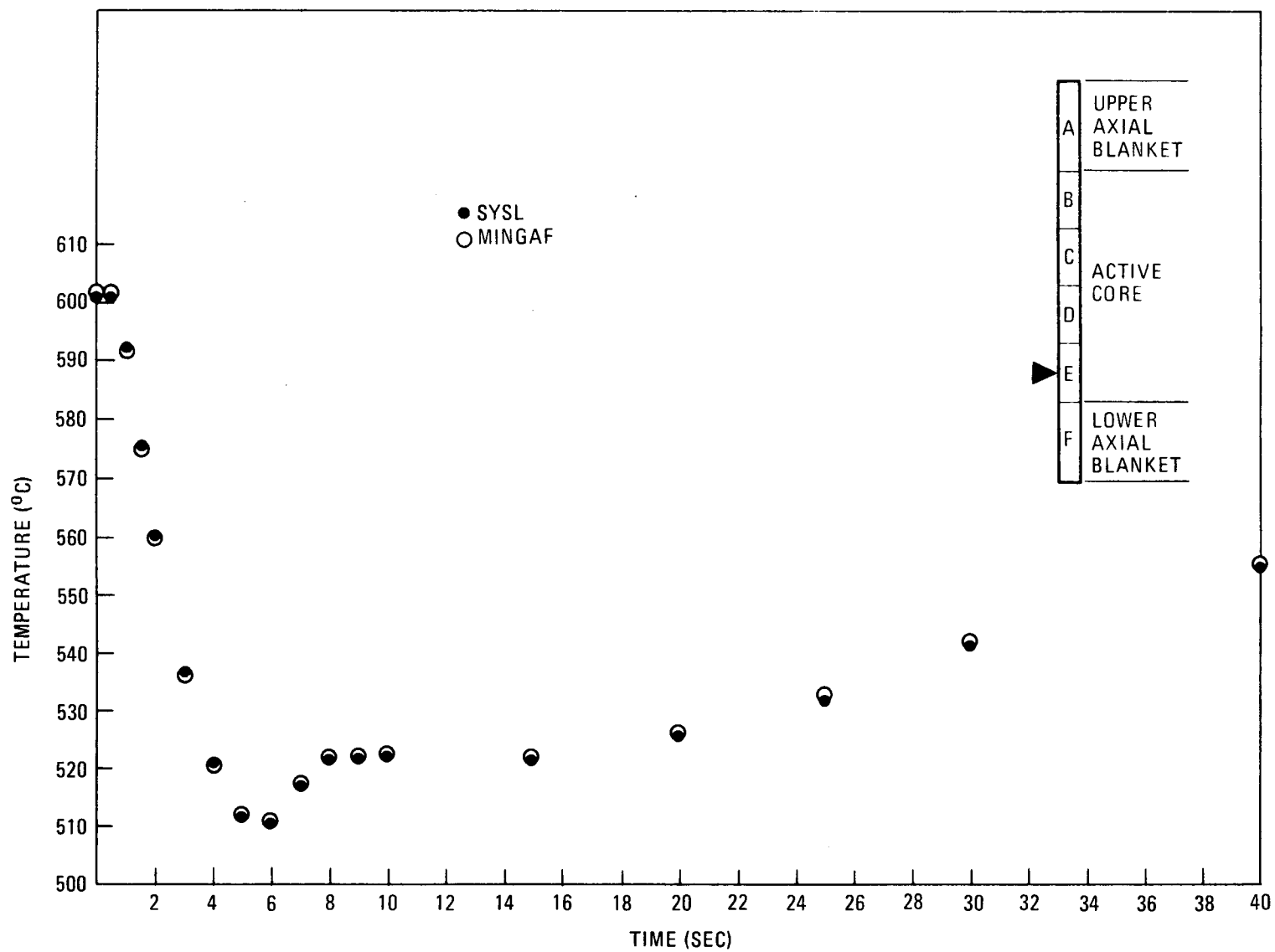


Fig. 4.4 Simulated GCFR gravity scram cladding temperatures - hottest region

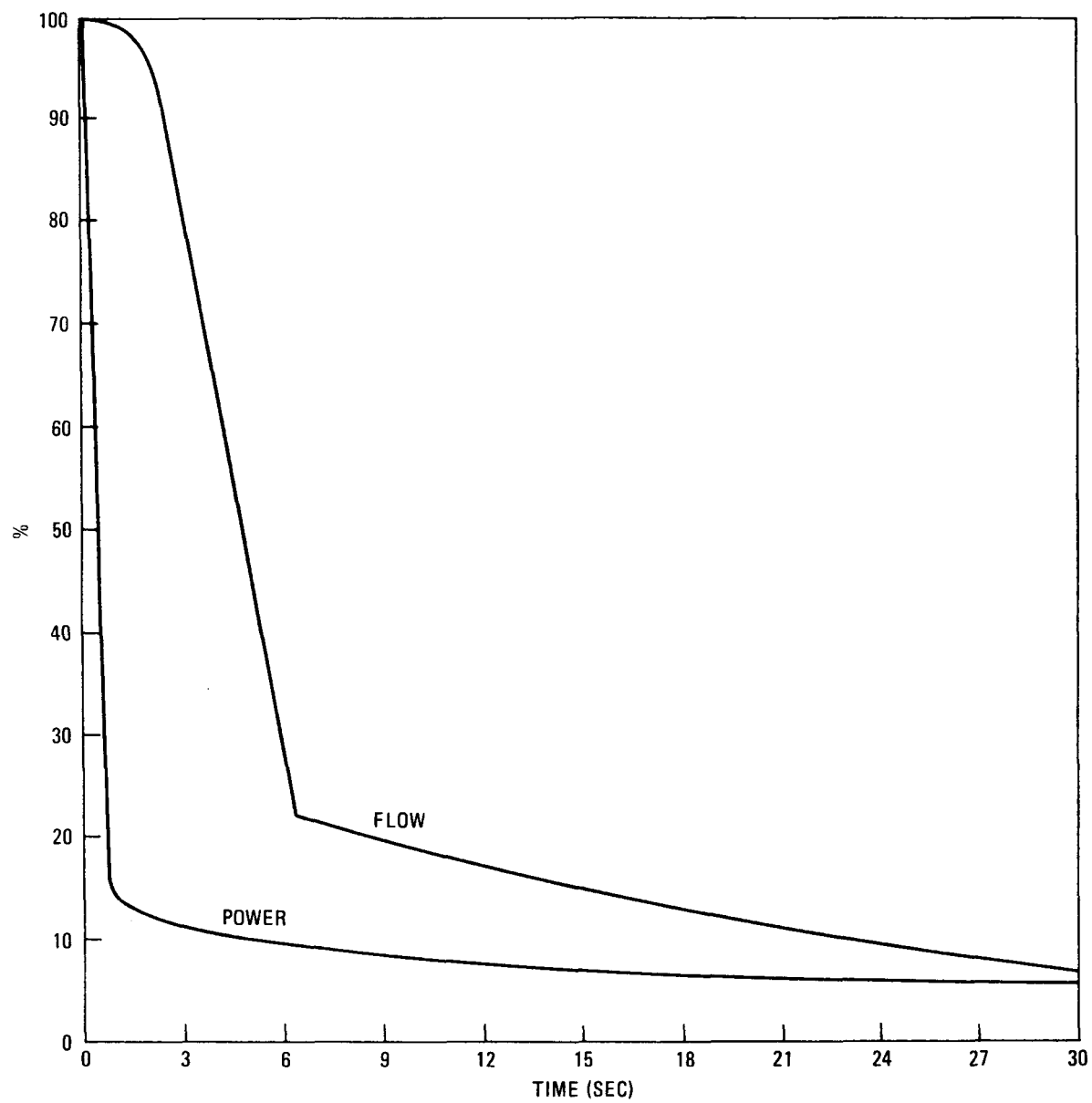


Fig. 4.5 Programmed power and flow rate to simulate GCFR scram in CFTL

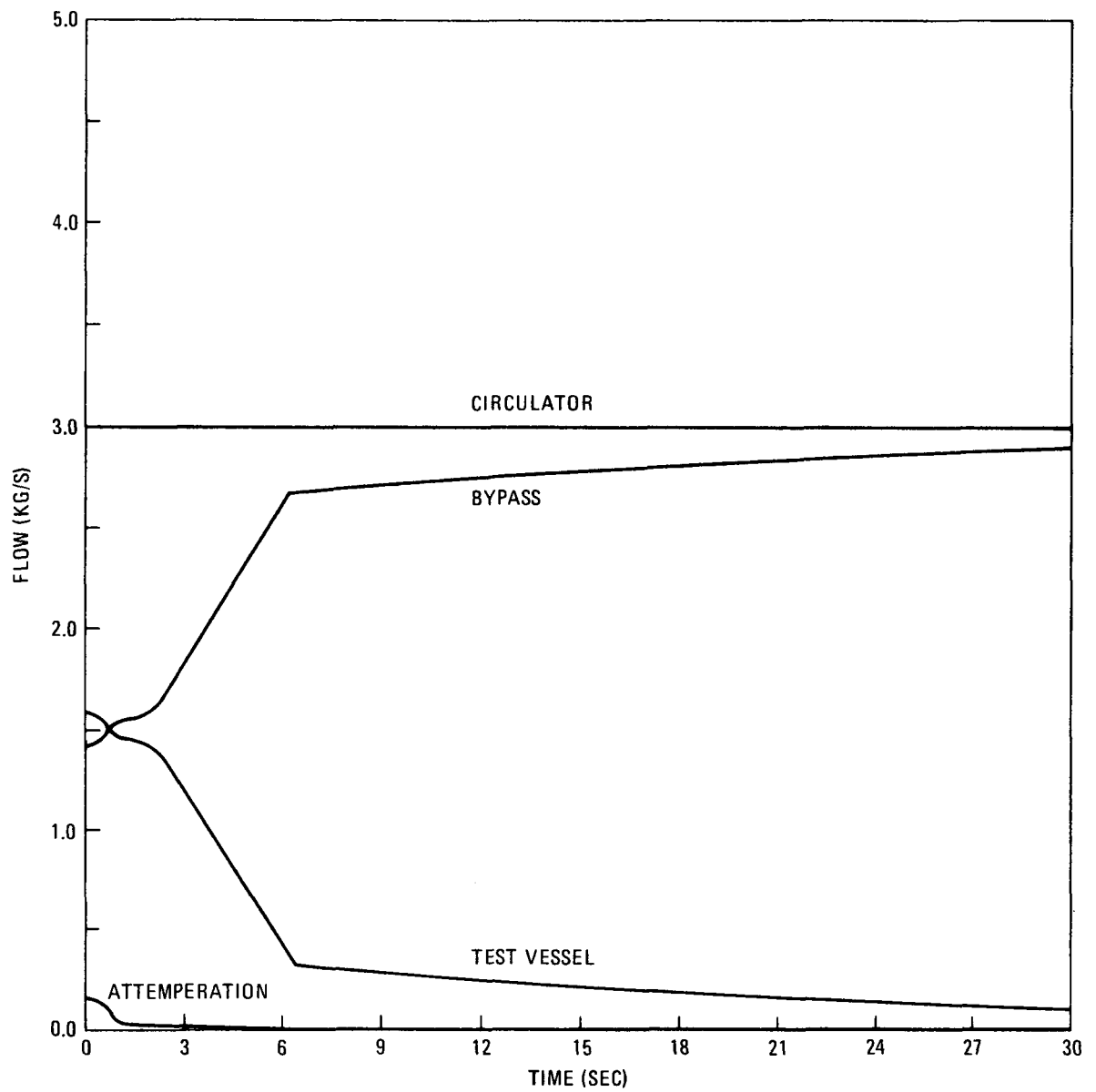


Fig. 4.6 Helium flow rates in CFTL for simulated GCFR scram

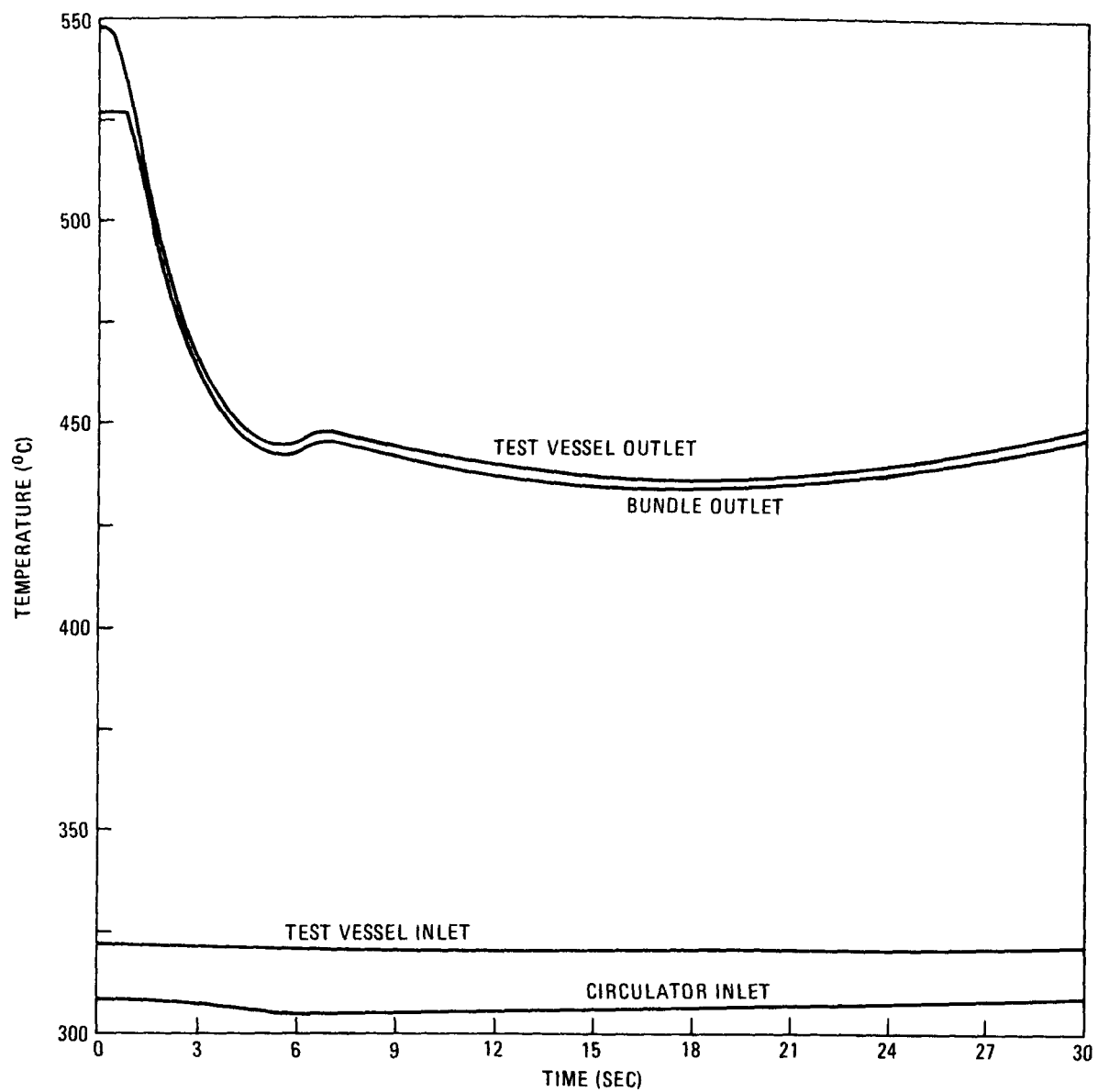


Fig. 4.7 Helium temperatures in CFTL for simulated GCFR scram

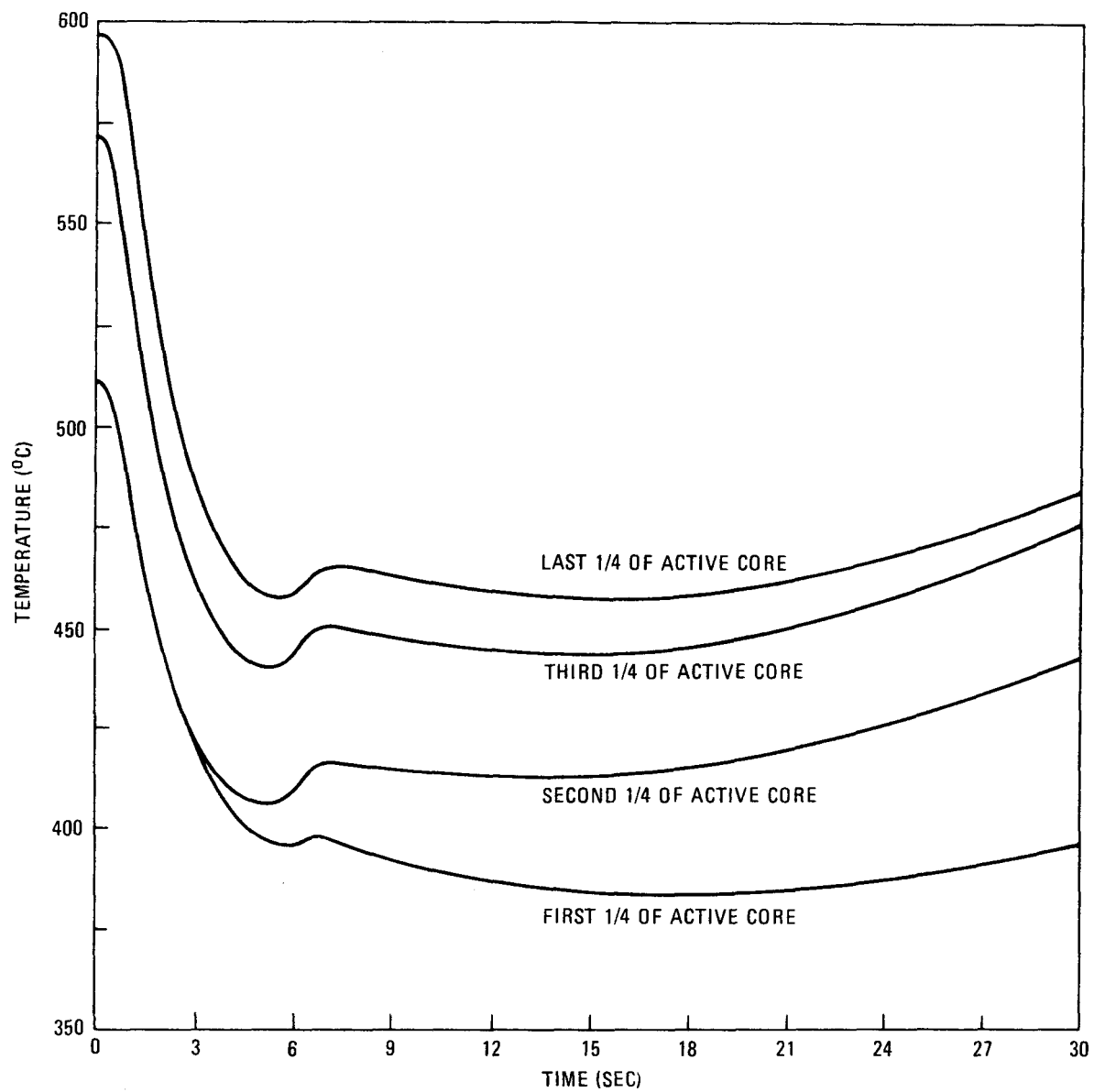


Fig. 4.8 Cladding temperatures in CFTL for simulated GCFR scram

studies involving repeated steady state runs, or runs involving model changes (which cannot be run from an absolute element), become expensive. To reduce the cost of doing these runs, a simplified model was developed in which 36 integrators were eliminated. This system has 10 integrators, 129 input parameters, and about 600 internal variables and is used to obtain approximate results, which may then be optimized using the full loop model. A modified version of this smaller code has been developed to automate computation of steady state heat exchanger air flows needed to obtain a given test vessel inlet temperature. Previously, this had been accomplished on a trial and error basis.

4.3. TEST SPECIFICATIONS

Upon review of comments on the sample "Outline Test Specification for GCFR-CFTL Priority One Series Tests," which was issued last quarter, a tentative test specification format with the following major sections or headings was prepared and distributed for review:

- Title and Issue Summary Sheet
- Table of Contents
- Introduction
- Test Requirements
- Data Format
- Description of Sensors
- Safety Requirements
- Special Requirements
- Quality Assurance Requirements

Upon completion of the review and resolution of comments and suggestions, the format will be used in preparation of the CFTL test specifications, the first of which is scheduled for issuance for review in February 1976.

4.4. TEST BUNDLE DESIGN

The GA and ORNL conceptual test bundle and test vessel designs were reviewed at a joint CFTL review meeting at ORNL. Proposed changes in GCFR fuel assembly design, such as the use of six unfueled corner rods to support the grid spacers, and the effects on CFTL test bundle design were discussed. This design change has a strong influence on the resolution to the question of an adequate test bundle size. Therefore, Tables 4.1 and 4.2 were prepared to show (1) a minimum but not necessarily sufficient test series (Table 4.1) and (2) an expanded test series to obtain the desired type and quantity of experimental data from the priority-one GCFR program for the changed GCFR fuel assembly design (Table 4.2).

Information is being developed on the space envelope required for the largest expected test bundle. This information is required input for ORNL's test vessel and loop design.

4.5. LIAISON WITH ORNL

A CFTL review meeting was held at ORNL on September 17, 18 and 19, 1975. Included in the three-day session were small group meetings during which the subjects of thermocouple error, heater development, and strain measurements were discussed in detail.

Information on ORNL development activities, both present and potential, was presented. Expected thermocouple errors for temperatures up to 800°C appear to be within acceptable limits. Strain measurements at temperatures to 590°C may be readily made, providing there is access to make the strain gage and leadwire installation. GA will define the structurally related measurement requirements. There appear to be no feasibility problems for the data acquisition system, helium purification system, and ultrasonic flow meters. ORNL is confident that the FFM pressure-boundary bundle design can be applied to CFTL, and that heater rod replacement is practical.

Table 4.1
MINIMUM PRIORITY-ONE TEST SERIES^a

Test Series	Test Series ID	Bundles	Major Test Areas
<u>Preliminary</u>			
37 rods, BOL	P-1	C	Loop hot-flow checkout, normal transients, depressurization margin.
	P-2	D	Steady-state undercooling margin.
	P-2	E	Transient undercooling margin.
<u>Fuel bundle</u>			
61 rods, BOL	F-1	H	Size effect, heat-transfer verification, normal transients, accident behavior, steady-state undercooling margin.
	F-1	I	Transient undercooling margin.
	F-2	M	Transient overcooling margin, depressurization margin.
61 rods, faulted	F-3	S	Detection of blockage-medium, local.
<u>Control bundle</u>			
54 rods, BOL	C-1	A	Normal transient, accident behavior, depressurization margin.
	C-1	B	Transient undercooling margin.
	C-2	F	Local blockage of control-rod guide-tube wall.
<u>Blanket assembly</u>			
127 rods, BOL	B-1	A	GCFR simulation, verification of heat transfer normal transients, accident transients, steady-state undercooling margin.
	B-1	B	Transient undercooling margin.

^aTotal bundles, 12; total rods, 771; spacer rods, 60; heater rods, 711.

Table 4.2
EXPANDED PRIORITY-ONE TEST SERIES^a

Test Series	Test Series ID	Bundles	Major Test Areas
<u>Preliminary</u>			
37 rods, BOL	P-1	C	Loop hot-flow checkout, normal transients, depressurization margin.
	P-2	D	Steady-state undercooling margin.
	P-2	E	Transient undercooling margin.
<u>Fuel bundle</u>			
61 rods, BOL	F-1	H	Size effect, heat-transfer verification, normal transients, accident behavior, steady-state undercooling margin.
91 rods, BOL	F-2	K	Heat transfer verification, size effect, normal transient, accident behavior, depressurization margin.
	F-2	M	Transient overcooling margin, backup for undercooling or depressurization margin.
91 rods, faulted	F-3	S	Detection of blockage-medium, local.
<u>Control bundle</u>			
54 rods, BOL	C-1	A	Normal transient, accident behavior, depressurization margin.
	C-1	B	Transient undercooling margin.
90 rods	C-2	F	Local blockage of control-rod guide-tube wall.
<u>Blanket assembly</u>			
127 rods, BOL	B-1	A	GCFR simulation, verification of heat transfer, normal transients, accident transients, steady-state undercooling margin.
	B-1	B	Transient undercooling margin.

^aTotal bundles, 12; total rods, 897; spacer rods, 60; heater rods, 837.

Operation of test bundles at low power and low helium flow is a potential problem in obtaining meaningful measurements because of loop heat loss and low differential pressure. GA will define the operating conditions and measurements that are necessary to satisfy the requirements for information on laminar flow of helium in the test bundles. The sodium bundle test program was reviewed. However, transfer of FFM technology to the CFTL program is limited because of the differing nature of the tests.

REFERENCES

1. Hopkins, H. C., Jr., "Program Plan for GCFR Core Flow Test Loop," USAEC, Report GA-A13080, General Atomic, August 1974.
2. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period May 1, 1975 through July 31, 1975," USERDA, Report GA-A13565, General Atomic, October 1975.
3. McAdams, W. H., Heat Transmission, 3rd ed., McGraw Hill, New York, 1954, Chapter 3, "Transient Conduction."

V. FUELS AND MATERIALS ENGINEERING (189a No. SU007)

5.1. OXIDE FUEL, BLANKET, AND GRID PLATE SHIELDING MATERIALS TECHNOLOGY

It is the responsibility of this subtask to maintain liaison with and surveillance of other ERDA and non-ERDA programs, especially the LMFBR program, to enable the utilization of all relevant information for the GCFR design. The areas included in this subtask are:

1. Oxide fuel technology,
2. UO_2 (axial) blanket technology,
3. ThO_2 (radial) blanket technology,
4. Grid plate shielding technology,

During this quarterly reporting period, an updated summary of GCFR fast-flux irradiations was provided to the national Fuel-Cladding Chemical Interaction Program steering group for use in their compilation of data on fast-flux irradiations and in planning for future programs of research and development in the area of fuel-cladding chemical interactions.

An evaluation of the results of the Kr-85 annealing technique for the determination of in-pile cladding operating temperatures is currently underway using data obtained under the irradiation task (see Section 5.4).

A review of the LWBR Environmental Impact Statement was started this quarter to obtain data on the irradiation of thorium as a radial blanket material.

5.2. CLADDING TECHNOLOGY

5.2.1. Mechanical Testing Program at ANL

The purpose of this program is to determine the effect of the following factors on the behavior and mechanical properties of GCFR cladding:

1. The effects of ribs, rib geometry, and fabrication technique,
2. The effects of impurity levels in helium typical of the GCFR,
3. The effects of end cap design and length of test specimens.

These tests are being conducted in a quasi-static helium environment at Argonne National Laboratory (ANL). General Atomic participates in an advisory, consultative and evaluative role. Testing under the initial test matrix (Ref. 1, p. 76) was initiated during the last quarter.

The initial test matrix was formulated with the objective of determining the effects of end cap design and the length of the specimen. The test matrix is given in Table 5.1 for reference.

Table 5.1
TEST MATRIX FOR INITIAL MECHANICAL PROPERTY TESTING AT ANL

Length (cm)	Number of Test Specimens		Temperature (°C)	Stress (MPa)
	ANL End Cap	HEDL End Cap With Reinforcing Collar		
4.45	4	4	650	248
9.83	4	-	650	248

The ribbed cladding specimens were fabricated by mechanical grinding from 20% cold worked type 316 stainless steel tubing (heat #88229). The environmental conditions were monitored, but not strictly controlled to high purity levels for these tests. Helium was used directly from the gas cylinder and was not further purified. The helium had ~70 ppm of H₂O, and

therefore the test specimens were oxidized by the atmosphere. The creep strain was measured periodically at the top of the ribs. Although the creep strain at the root diameter is the more important parameter, it was not measured in these tests due to the lack of a suitable measuring device at that time. Later a thread-type micrometer was modified for this purpose and appears to be performing satisfactorily at GA. Another micrometer will be modified for the metric tubing now on order.

The test results as received from ANL are shown in Table 5.2. Ten specimens failed by pinhole type of failure within 815 hours. One specimen failed due to defective welding early in the test. Another specimen was intentionally allowed to fail by catastrophic rupture, which occurred at 851.3 hr due to leaving the one liter gas volume connected to the specimen: the rupture encompassed 3 ribs. The following are the important findings from these tests:

1. The 9.83 cm long specimens without the reinforcing collars failed early in the test program.
2. The failures in the specimens without the collars were very close to the welded end cap.
3. The failures in the specimens with the collars were nearer the center.
4. The specimens without collars bulged to the shape of a dog bone, and the specimens with collars bulged in the center.
5. Microcracks are more abundant in the transition region between the ribs and the root diameter. The microcracks are in the longitudinal direction.
6. One or more ribs were involved in the failures. A statistical analysis of the test results was performed at General Atomic using the Weibull technique and the conclusions are given below:

Table 5.2
BIAXIAL CREEP RESULTS FOR ROUGHENED GCFR CLADDING AT 650°C

Specimen Number	Specimen Group ^a	t_f (hr)	Type of Failure ^b	$\delta_{\max}^c$ (%)	δ_{avg}^d (%)	Calculated EOD ^e (μm)
S7-4	B	576.7	F	7.46	2.69	4.2
17-5	C	581.8	F	8.04	2.43	3.4
16-7	C	636.8	F	5.50	2.69	2.4
S6-3	B	655.3	F	5.82	1.80	2.1
17-2	C	658.9	F	7.46	4.06	1.3
15-7	C	658.9	F	4.50	3.51	1.5
S6-6	B	695.8	F	6.32	2.21	1.2
16-2	A	727.9	F	5.72	4.96	1.2
18-2	A	737.3	F	7.98	6.56	--
18-3	A	814.9	F	8.04	6.61	--
S6-4	B	851.3	F,R	12.96	2.73	--

^aSpecimen groups: A = 4.45 cm long with end collars; B = 4.45 cm long without collars; and C = 9.83 cm long without collars. Specimen S7-4 was roughened by chemical etching, all others were roughened by mechanical grinding.

One Group A specimen (15-5) was removed from the test after 242 hr when a leak developed in a weld closure. Specimen S7-4 was excluded from the present analysis, since it is chemically etched rather than mechanically ground.

Based on root diameter and using the thick wall formula, the Groups A and C specimens were stressed at 247 MPa and the Group B specimens were stressed at 244.9 MPa. These are nominal values.

^bF = intergranular fissure, and R = rupture.

^c $\delta_{\max}$ = diametral strain at failure site.

^d δ_{avg} = average diametral strain.

^eEOD = equivalent orifice diameter.

- a. No statistical difference is seen between the three groups of specimens based on the time to failure and the maximum strain at failure. In other words, there is no difference between the two different lengths or the end cap designs based on time to failure or maximum strain at failure.
- b. A statistically significant difference exists between the two end cap designs, based on the average strain at failure. The HEDL end cap design with reinforcing collars is superior to the ANL end cap design based on the average strain. A statistical analysis based on a random block technique performed by ANL also arrived at the same conclusions.

Post test analysis of the specimens indicated that all failures were in the 20% cold-worked matrix, although some failures were very close to the weld zone. It appears that the specimens without the collars were weakened by the end caps. Based on the above, all future tests will use specimens with reinforcing collars of the HEDL design. The results are well within the error bands of data in the "Nuclear Systems Materials Handbook."

Delivery of a new lot of tubing ordered from Carpenter Technology was delayed due to various causes. Initially the tubing order was rejected because of lower than minimum specified yield strength of the finished product. Additional tests were conducted, and the tubing was accepted based on the following considerations:

1. The tubing was purchased in accordance with RDT M3-28T specification. The minimum yield strength (YS) for the finished product was specified to be 448 MPa at 537°C. The vendor had requested and received permission from ANL to reduce the minimum yield strength requirement to 413.7 MPa. This reduction is now considered acceptable based on HEDL experience.

2. Initially, three tensile tests were conducted: two yield strength values were > 413.7 MPa and the third was 395 MPa. Upon re-examination of the stress strain curves, it was found that the yield strength value should have been 410 MPa instead of 395 MPa, which is very close to the acceptable minimum.
3. Three additional tensile tests were conducted using extensometers at a slightly different magnification. The yield strength values were 34.5 MPa higher than initial values in general. Such a small change in the testing technique having such a significant effect on the results indicates the need to specify the measuring technique to be used in the future.
4. Three more tests were conducted using the HEDL testing technique in which cross head motion was used to measure strain. The yield strength values obtained by this technique were about 69 MPa higher than the other results. This technique was used to obtain the data presented in the "Nuclear Systems Materials Handbook." Experience at HEDL indicates that their results are ~ 34.5 MPa higher in general than those from Atlas Testing Laboratories, who tested the tubing for the ANL order. Although different strain rates were used in these tests, HEDL's experience⁽²⁾ indicates no effect of strain rate.
5. It is believed at HEDL that there is not a firm basis for any relationship between yield strength and long term creep properties, although the eight lots of experimental tubing results⁽³⁾ indicate such a relationship.
6. Based on discussions with HEDL, which emphasized the variation between lots, it was decided that GA should have its own controls. The experimental program at ANL, as planned at present, does contain control specimens in the test matrix. The tests are to be conducted in purified helium and in helium containing impurities

typical of the GCFR, and the results will then be compared. This comparison approach is justified since the same lot of tubing will be used for both tests. A true comparison of ANL test results against LMFBR data is difficult due to (1) variation between lots and (2) uncertain environmental conditions used in the generation of the LMFBR data.

Since mechanical grinding is the GA reference rib fabrication technique, 10 meters of tubing have been shipped to KWU for rib fabrication. In addition, 11 meters of tubing have been shipped to EIR for fabrication by the electrochemical grinding technique. The ribbed tubing is expected by the end of November and will be used in the tests expected to start in January 1976.

Because of these delays, an interim test matrix was designed with concurrence from ANL. This test matrix is shown on Table 5.3, and the following considerations were taken into account in the formulation:

1. Available tubing has been used in the formulation of this interim test matrix.
2. The test matrix is designed to determine the difference between the ribbed and smooth cladding, where the outside diameter of the smooth cladding is equal to the root diameter of the ribbed cladding.
3. The test matrix will also attempt to determine the difference in the mechanical properties between the etched and mechanically ground cladding.
4. Although the test matrix includes specimens of 9.83 cm and 4.45 cm lengths, the effect of length is not under study. Most of these specimens have already been fabricated, and they are included to improve the statistics.

Table 5.3
GENERAL TEST MATRIX FOR THE INTERIM TESTS

Group	Specimen Type	Specimen Quantity ^a	Nominal Length (cm)	Nominal ID (cm)	Hoop Stress ^b (MPa)	Temp (°C)
A	Chemically etched Smooth	8 or 10	9.83	0.668	238 ± 2.7	650
B	Chemically etched Ribbed	8 or 7	4.45	0.668	238 ± 2.7	650
C	Chemically etched Smooth	8 or 7	4.45	0.668	238 ± 2.7	650
D	Mechanically ground Ribbed	4	4.45	0.711	238 ± 2.7	650
E	Mechanically ground Smooth	4	4.45	0.711	238 ± 2.7	650

^aNumber of specimens in Groups A, B, and C will depend on the welding success of Groups B and C.

^bThe stress values are based on nominal cladding dimensions.

5. All specimens will have weld reinforcing collars. Split collars will be tack welded on to specimens that have been fabricated without collars.
6. The internal pressurization on the different diameter specimens will be adjusted to provide the same hoop stress. The computation of the hoop stress will be based on the root diameter of the ribbed specimens and the outside diameter of the smooth specimens.
7. The pressurizing and the environmental helium gas will be purified by passing through a liquid nitrogen cooled, activated charcoal trap. The trap will be designed to remove water vapor and oxygen from the gas stream for the duration of the test.

5.2.2. Helium Loop Test Program at PNL

The primary objective of this program is to permit comparison of mechanical properties in recirculating helium at Pacific Northwest Laboratories (PNL) to those in quasi-static helium, measured in the Mechanical Properties Testing Program at ANL (see Sec. 5.2.1). The scope of work has been agreed upon, and the evaluation of techniques for detection of pin hole leaks and ruptures has been completed.

Work during the previous quarter was directed toward completion of the modifications to the loop for unattended operation, including high temperature protection circuits and impurity monitoring instrumentation. Agreement was also reached on an initial test matrix for the PNL helium loop test program with PNL personnel.

During this quarter, the PNL helium loop was modified to accommodate the injection of water and hydrogen. This was done by separating the CuO tower from the integral loop purification system and using this tower in the bypass system. The impurity monitoring system, i.e., the Panametrics moisture probes and the Thermox meters, have been installed and connected to the loop piping.

Initial loop operation was aimed at determining whether a temperature of 650°C (1200°F) could be achieved and maintained by the use of nichrome heaters alone. This was accomplished without any difficulty. Long time operation with the desired impurities was the next goal. For this purpose, the desired H₂ and H₂O levels were established, and the loop was held thermally stable for three hours. During this time the loop conditions changed from 41 ppm of H₂O and an H₂/H₂O ratio of 2.50 to 24 ppm of H₂O and a H₂/H₂O ratio of 4.95, most likely a direct result of the oxidation of the loop internals. In the next phase of operational testing the loop was operated in an attended mode of 2.07 MPa and 649°C for five days. It was found that to maintain the desired impurity levels a small bypass flow through the CuO tower and a small makeup flow of helium and 230 ppm hydrogen mixture was required.

The environmental conditions for the initial tests at PNL were

H₂O - 86.1 Pa,
H₂ - 304 Pa,
Others - negligible.

Some ribbed and etched smooth (root diameter) tubing was fabricated by chemical etching. However, the etched smooth tubing was found to have a periodic rib-like structure of 5.1 to 7.6 μm height. This was considered to be unacceptable for these tests and was not forwarded to PNL. A new lot of ribbed cladding is expected by the end of November and will be forwarded to PNL; the testing is expected to start by February 1, 1976.

The loop modifications are complete, and the loop is ready for testing.

Information was received on the simulation of the steam in-leakage accident, which was expected to be a one time test with about 2% steam in the loop for about one hour. However, this simulation test will be hindered by the molybdenum heaters in the loop. Based on a surface temperature of

700°C, a maximum of 40 gms of molybdenum from the inner surface of the heaters will be lost. This molybdenum will be transported in the form of MoO_3 to cooler parts of the loop and may also deposit on the test specimens. MoO_3 leads to catastrophic oxidation of stainless steel. The molybdenum loss is significant enough to be detrimental to the future operation of the loop. Based on these considerations, the steam in-leakage accident simulation is not considered advisable in the PNL helium loop. It may be easier to perform this test in the quasi-static test rig at ANL or even more desirable would be the small helium loop at PNL.

5.3. F-1 FAST FLUX IRRADIATION EXPERIMENT

Irradiation of the encapsulated seven-fuel-rod F-1 (X094B) experiment has now achieved a maximum burnup exposure of 106 MW/kg (11.6 at -%). Irradiation will continue to the burnup goal for the experiment of 125 MWD/kg (13.4 at -%).

The postirradiation examination work at Argonne National Laboratory-East (ANL-E) on the five F-1 rods removed at 50,000 MWD/Te is completed and the final report is in final internal review at ANL-E.

The postirradiation work underway or remaining to be done at GA is outlined below. Work at GA during this quarter was aimed at capsule and cladding operating temperature determination, tritium yield measurements from dosimeters, and charcoal trap analysis.

5.3.1. Operating Temperature Determination

Arrangements have been made with another group at GA for the annealing and measuring of the SiC to determine temperatures in the thermal barrier of the F-1 capsules (G-1, 2, 5, 6, and 7).

Work continues on the determination of end of life cladding operating temperatures employing the Kr-85 postirradiation annealing technique. Additional cladding sample sections from G-6 and G-2 rods have been examined

at GA using the Kr-85 anneal technique. Only three or four more pieces are left for analysis. These have been reserved for concurrent analysis of the absolute Kr-85 inventory during an anneal run. The observed temperatures for G-6 range from 609°C in the lower fueled region to 783°-776°C in the midplane of the fueled region. This, incidentally, is the highest measured i.d. temperature of any of the F-1 rod samples. The observed temperatures in the G-2 rod samples remain in the 600°C range, over 100°C lower than the calculated temperature and indicative of cladding attack interference.

5.3.2. Tritium Yield Determination

Tritium (H-3) yield determination from dosimeter particles from the F-1 capsules awaits completion of the work on U-238 and Th-232 particles, which has been delayed owing to manpower priority shifts onto other tasks. U-235 yield data is still delayed due to the non-resolution of reference counting data differences: a 30% error exists on reference isotopes obtained by gamma counting.

5.3.3. Charcoal Trap Analysis

Metallographic examination of the G-6 active trap cladding at the inlet (371°C) to the lower and upper traps (~ 500°C) indicated no attack by fission products or carburization of the cladding inner surface. These results are illustrated in Figs. 5.1 and 5.2.

Preliminary radiochemical results on the first two sections of the G-6 lower trap show detectable quantities of Sr-90. Relative to Cs-137 content in these sections the Sr-90/Cs-137 ratio is ~ 0.01. The Cs-137 content in the first section is 5.4×10^{13} atoms/mg charcoal (10^{-5} mg/mg). The total cesium loading is then about 2×10^{-2} mg/mg charcoal or about 2%. (The Sr-90/Cs-137 GB-9 ratio was 3.2×10^{-3} or a factor of three lower.) Sr-89 probably will not be detectable owing to the 2.5 years elapsed since the end of irradiation. This is a long time compared to the 50.4 day half life of the isotope, which will translate to a decay factor ($e^{-\gamma t}$) of 2.4×10^{-6} since EOL.

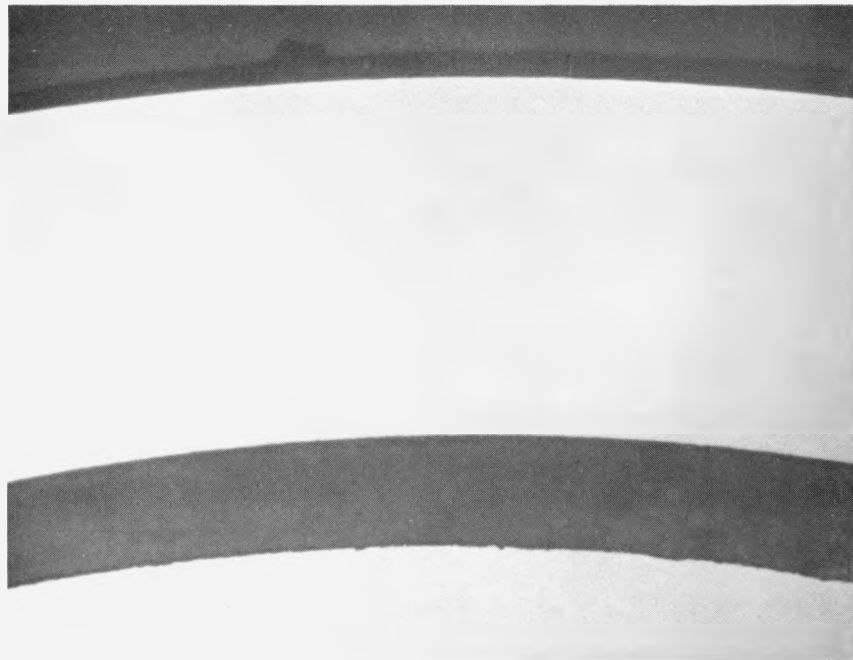


Fig. 5.1 Transverse section of lower G-6 trap cladding, 20% cold worked 316 SS cladding tube, operating temperature 375°C (Note, no evidence of carburization or attack of the cladding by fission products.)

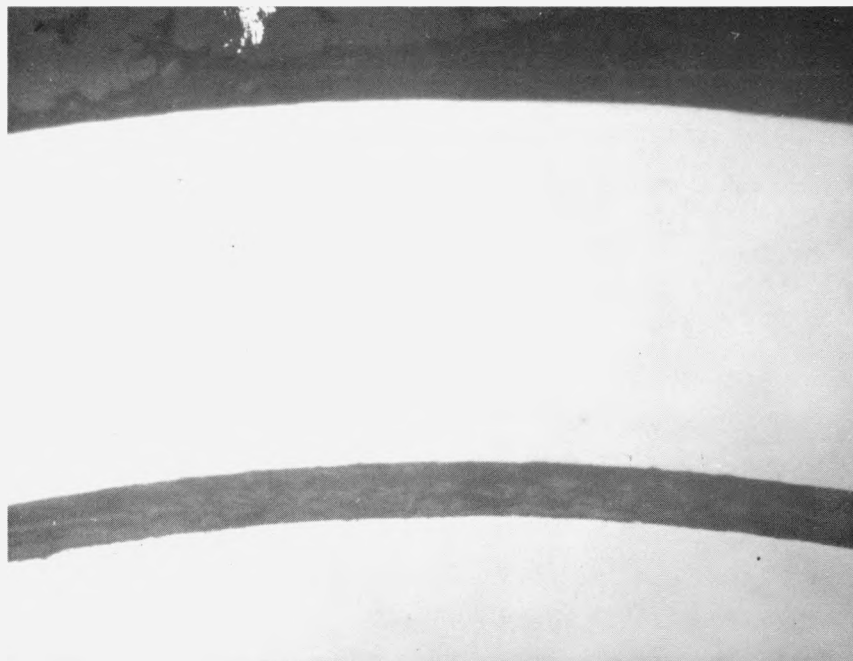


Fig. 5.2 Transverse section of upper G-6 trap cladding, 20% cold worked 316 SS cladding tube, operating temperature 500°C (Note, no evidence of carburization or attack of the cladding by fission products.)

As previously reported,⁽⁴⁾ charcoal sample aliquots were selected from each axial trap section from rod G-6 and were shipped to Idaho Nuclear Corp. (INC) for specific fission product analyses. These analyses will provide quantitative isotopic data for cesium and rubidium species and for total cesium. However, INC has experienced some difficulty in quantitative analyses of cesium and rubidium. Their first analysis was performed on leached charcoal samples that provided less than quantitative recovery of the fission products. This should not affect the relative isotopic concentrations but will affect the quantitative determination. This is being re-examined by their analytic chemistry group. GA has had similar difficulties in leaching strontium (and cesium).

Resolution of counting data differences for Cs-137 between GA and INC has not yet been accomplished. Final results from a third counting lab have not yet been received. Radioactivity solution standards were sent from GA to INC. The difference between these two labs is ~ 10% for two Cs-137 and one Co-60 sample standard solutions, with GA biased on the positive side. Interestingly, this is a smaller difference than was observed (~ 27%) on comparing the charcoal sample activity. It is essential to resolve these differences since cesium loadings in the charcoal traps, tritium yields, and fast and thermal neutron fluences are dependent on the counting data.

5.4. FAST-FLUX IRRADIATION EXPERIMENT F-3

The irradiation of the F-3 experiment started in EBR-II, Run 75 on December 13, 1974, and to date has reached an exposure of 30 MWd/kg (3.3 at -%) of a goal exposure of 100 MWd/kg (10.9 at -%). As previously reported,⁽⁵⁾ these capsules have been designed for irradiation in an EBR-II core position (row 4) and share a type J19A subassembly (designated X206) with an ANL Group-08 high-temperature chemistry experiment. The experiment will be irradiated to a burnup of 50 MWd/kg burnup, or 300 full-power days, at which time a planned interim examination will occur (c. February 1976). The final maximum burnup and neutron exposure goal for the experiment is 100 MWd/kg (11.0 at -%), 1.5×10^{23} n/cm². The experiment is being irradiated in location 4B3 of the EBR-II core.

It has been proposed by ANL that the interim examination of F3-08 (X206) be begun at the end of EBR-II, run 80 or mid-run 81 to avoid interference with a rush of high priority examinations that will occur at the end of run 81, when the long shutdown for annual maintenance of EBR-II begins. General Atomic concurs with this proposal, and ANL-E will transmit the request to the EBR-II project.

Because of other high priority work, ANL-E reports that the fabrication of the three F-3 replacement rod capsules, to be installed in X206 after the interim examination, has been limited to date to adjusting the O/M ratio in the fuel and blanket pellets, but that completion of fabrication is expected to occur as originally scheduled.

5.5. F-5 PROTOTYPE IRRADIATION EXPERIMENT

The F-5 prototype is for experimental study of the performance of fuel rods irradiated under simulated GCFR conditions to high burnups for the purpose of determining the reliability of the GCFR fuel rod design and to discover what failure modes may exist, as well as studying the effect of a step power increase that simulates the 180° rotation of a subassembly at the core-blanket interface in the proposed GCFR demonstration plant. A few rods with modified fuel-blanket interfaces (mixed oxide-uranium dioxide interface) will be irradiated to study methods of mitigating the potential detrimental reaction between cesium and the blanket pellets at the interface.

The "Request for Approval in Principle" for the F-5 experiment was submitted to RRD by ANL-E on September 24, 1975.

The cladding tubing ordered by ANL-E for the F-5 experiment has been received and is in a controlled storage area at GA.

5.6. GB-10 VENTED FUEL ROD EXPERIMENT

The release and transport of fission products in GCFR fuel are being measured and studied in capsule GB-10, which is being irradiated in the

Oak Ridge Research Reactor (ORR). The burnup of the pressure-equalized and vented fuel rod in capsule GB-10 has reached approximately 75 MWd/kg (8.2 at -%) of the exposure goal of 100 MWd/kg (10.9 at -%). The first 27 MWd/kg were accumulated at a heat generation rate of 39.4 kW/m (12 kW/ft) at a cladding outside surface temperature of 565°C; the remainder have been accumulated at 44.3 kW/m (13.5 kW/ft) at a cladding outside surface temperature of 630°C.

Activity during this reporting period was expended principally in supporting the tritium measurements being conducted at ORNL. These included GA monitoring, consultation, and observation of the tritium monitor calibration and debugging tests and tritium venting experiments at ORNL, design and construction of a high pressure water saturator, laboratory tests to define operating conditions for the magnesium converter, and development of a method for sampling low levels of H₂ using a liquid-nitrogen-cooled charcoal trap at GA.

5.6.1. Tritium Monitor Calibration and Debugging

A series of calibration and debugging tests was conducted at ORNL using the tritium monitoring instrumentation installed on the sweep-gas system of capsule GB-10. The tests uncovered two unexpected developments: (1) a transient response to tritium passed through the magnesium converter (HTO → HT) not observed in the laboratory tests, and (2) the appearance of an interfering ionizing radiation that was delayed for shorter times than those for hydrogen in the liquid nitrogen cooled charcoal trap. Laboratory tests and analytical calculations were conducted at GA during the period to help overcome these problems with results given below.

5.6.1.1. Magnesium Converter. Qualification testing of the magnesium converter used in the tritium monitor at the capsule had been conducted with tritium plus 4% normal H₂ in the helium carrier gas. Total hydrogen (tritium plus normal hydrogen) in the capsule and monitor were known to be less than 50 ppm and could be as low as 1 ppm. It was postulated that at low

total hydrogen levels the tritium was being trapped in the magnesium converter. A laboratory test in confirmation was undertaken.

Tritium gas with a low ($\sim 10\%$) normal H_2 content was passed through a magnesium converter and an ion chamber with an attached electrometer. The magnesium converter consisted of alternate sections of magnesium chips and 20 mesh magnesium granules, held at both ends by SS wool in a tube 15.8 mm in diameter and an active length of 273 mm. There were 11.8 g of chips and 36.5 g of granules.

The converter was operated under a number of operating conditions. In the cold condition it took 6.5 hours for the converter to reach a steady effluent level of 85% of the input. At its normal operation temperature of 500°C , it took seven hours, but the effluent was only 55% of influent level.

Stripping the converter with non-tritiated helium containing 8 ppm H_2 was ineffective. However, with 500 ppm of H_2 in helium, the tritium produced a peak of more than 11 times the influent level previously used.

Returning to tritiated helium but with H_2 added to make 1000 ppm, the effluent increased to 90% of influent and reached this level in a short time.

These results verified that the magnesium converter required significant levels of H_2 for proper operation. This was subsequently confirmed with in situ tests at the capsule.

5.6.1.2. Ne-24 Interference and Source. During operation of the tritium monitor on capsule GB-10 a significant ion chamber output signal was observed almost immediately after gases swept from the capsule flowed into the tritium monitor. This was contrary to expectation since tritium in carrier helium was expected to be first to break through the liquid-nitrogen-cooled charcoal trap. Measurements were to be completed before the next activity (argon) would come through, and then the trap would be regenerated. Neon, an anomaly from the pattern of increasing delay with increasing molecular weight, is the

only gas other than helium to penetrate the trap before hydrogen and tritium. An investigation into sources of neon activities was therefore conducted at GA.

Initially the neon impurity in helium was considered to be a likely source of Ne-23 (38 sec half-life). However, analysis of the ion chamber decay curve at ORNL showed activities of ~ 4 minutes and 15 hours. The 15-hour activity remained in the ion chamber when the chamber was purged with clean gas and thus was deposited in the chamber. Ne-24 (3.38 minute half-life) and Na-24 (15 hour half-life) the daughter product of the Ne-24, were suspected. However, the source of Ne-24 was unknown and became the subject of further analyses.

Several possible sources for the nuclear production of Ne-23 and Ne-24 isotopes in the GB-10 fuel capsule were explored, as the interfering activities related to the on-line tritium ion chamber measurements. Consideration was given only to the elemental impurities in the GB-10 fuel. Magnesium and sodium are the only major relevant impurities giving rise to the nuclear isotopic reactions that produce Ne-23 and Ne-24.

In fact, impurity analysis for magnesium in the fuel made during fuel fabrication indicates the measured level exceeds the specification level by about a factor of 5.

The impurity levels in the fuel are:

Magnesium, 250 ppm in 100 gram oxide fuel = 2.5×10^{-2} grams

Isotopic abundance of Mg-24 is 78% and of Mg-26 is 11%

Mg-24 = 4.5×10^{20} atoms

Mg-26 = 6.9×10^{19} atoms

Sodium, 100 ppm in 100 gram oxide fuel = 1.0×10^{-2} gram

Na-23 = 2.6×10^{20} atoms

$$N \left(\frac{\text{atoms generated}}{\text{sec}} \right) = \sigma N \phi ,$$

where σ = microscopic cross-section for a specific reaction in the neutron flux spectrum present (barns),

N = number of target atoms exposed (atoms),

ϕ = neutron flux ($\text{n/cm}^2 \cdot \text{sec}$).

For the Mg-26 (n, He-3), Ne-24 reaction.

$$N = (0.1 \times 10^{-3}) \frac{\text{barns}}{\text{atom}} \times 10^{-24} \frac{\text{cm}^2}{\text{barn}} (6.9 \times 10^{19}) \text{ atoms} (3 \times 10^{12}) \frac{\text{n}}{\text{cm}^2 \cdot \text{sec}} = 2.07 \times 10^4 \frac{\text{atoms}}{\text{sec}} ,$$

or 0.6 μCi .

The neutron flux incident on the GB-10 capsule was obtained from the ORNL Operations Division and is shown in Table 5.4.

Table 5.4
NEUTRON FLUX INCIDENT ON CAPSULE GB-10

	Peak	Average
ϕ_{th}	1.2×10^{14}	5.4×10^{13}
$\phi_1 > 1 \text{ MeV}$	5.5×10^{13}	2.5×10^{13}
$\phi_2 > 2.9 \text{ MeV}$	1.3×10^{13}	5.9×10^{12}
$\phi_3 > 4.1 \text{ MeV}$	6.4×10^{12}	2.9×10^{12}
$\phi_4 > 8.1 \text{ MeV}$	3.1×10^{11}	1.4×10^{11}

Other activities (calculated) resulting from the presence of magnesium, sodium, and neon are shown in Table 5.5.

Table 5.5
CALCULATED ACTIVITIES RESULTING FROM PRESENCE OF Mg, Na, AND Ne

Reaction	Cross-Section	Flux (n-cm ² /sec)	Activity (μCi)	Significance
Mg-24 (n,p) Na-24	0.1 b	3 x 10 ¹²	a	a
Na-24 (n,p) Ne-24	0.1 b	3 x 10 ¹²	9 x 10 ⁻⁵	Neg
Mg-26 (n,He -3) Ne-24	0.1 mb (assumed)	3 x 10 ¹²	0.6	Great
Na-23 (n,p) Ne-23	10 mb	3 x 10 ¹²	a	a
Ne-23 (n,γ) Ne-24	0.1 b (assumed)	5 x 10 ¹³	1 x 10 ⁻⁶	Neg
Ne-23 (n,γ) Na-24	0.5 b	5 x 10 ¹³	a	a
Na-24 (n,p) Ne-24	0.1 b	3 x 10 ¹²	4.2 x 10 ⁻³	Small

^a Interim product of two step reaction

Assuming all the Ne-24 atoms are released from the fuel into the sweep gas, the activity concentration in the gas in the ion chamber is

$$A_o (\mu\text{Ci/ml}) = \frac{\lambda N}{V}$$

where λ = decay constant (sec⁻¹),

V = volumetric flow rate through the ion chamber.

$$A_o = \frac{0.693}{3.38} \times \frac{2.07 \times 10^4}{750} \times \frac{24.7}{14.7} \times \frac{1}{3.7 \times 10^4} = 2.57 \times 10^{-4}$$

min	atoms sec	min ml STP	ml STP ml ion chamber	sec · Ci atoms = μCi ml
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It has been estimated from previous measurements and the addition of monitoring equipment that the time in transport between the fuel rod and the ion chamber is ~ 1.5 min at 750 ml STP/min:

$$A = A_0 e^{-\lambda t}$$

$$A = 2.57 \times 10^{-4} e^{\frac{-0.693}{3.38} \cdot 1.5}$$

$$= 1.89 \times 10^{-4} \text{ } \mu\text{Ci/ml}$$

This result is comparable to the concentration of $4 \times 10^{-4} \text{ } \mu\text{Ci/ml}$ required to generate the observed ion chamber output of 450 mV. Considering the assumptions of cross-section, release from the fuel, and neutron flux, the result gives credence to magnesium in the fuel as the source of the Ne-24.

Since there is no way to eliminate the source of the Ne-24, its effect on the monitor's capability was estimated. Under the conditions that the Ne-24 was measured (i.e. maximum flow of sweep gas through the fuel region of the rod at ~ 12.8 ml STP/sec, and 20 mV cyclic variation in ion chamber output corresponding to $\sim 3^\circ\text{C}$ peak-to-peak temperature cycling of the charcoal trap with a 10 min period), a slow updrift of the output was observed, which was attributed to the Na-24 daughter-product of Ne-24. Under these circumstances, a signal from tritium of ~ 50 mV would be the minimum discernable amount, superimposed on the 450 mV Ne-24 signal. This corresponds to $5 \times 10^{-5} \text{ } \mu\text{Ci/ml}$ of tritium in the ion chamber. The expected value at a power level of 44 kW/m (13.5 kW/ft) and a flow rate of 200 ml STP/min was calculated to be $2.23 \times 10^{-3} \text{ } \mu\text{Ci/ml}$. Correcting to the measuring conditions,

$$2.23 \times 10^{-3} \frac{39}{44} \frac{33}{12.8} = 5.15 \times 10^{-4} \text{ } \mu\text{Ci/ml} \quad .$$

Thus, the Ne-24 signal increases the minimum level of tritium measurable within the ion chamber from $\sim 0.25\%$ to 10% .

In the measurements of tritium venting without the addition of H_2 to the capsule sweep gas, the levels have been only $\sim 5\%$ of the expected values and therefore below the threshold of the monitor with the maximum Ne-24 interference. By reducing the sweep-gas flow rate, the neon signal is rapidly reducible. The importance of high flow rate to the measurement of tritium has not yet been measured, but it may be related to the rate of tritium permeation through the cladding.

5.6.2. Hydrogen Samplers

Because of the low hydrogen concentration expected in the sweep gas of capsule GB-10, it was necessary to devise a reliable way of sampling, concentrating, and measuring the hydrogen content of the gas. Two charcoal trap samplers have been designed, fabricated and tested at GA. A procedure for their use has been developed. Figure 5.3 is a photograph of one of the samplers.

The trap consists of a bed of 0.56 g of charcoal in a U-tube that is immersed in liquid nitrogen. H_2 from the gas stream is first concentrated on the trap at a measured flow rate over a measured time period. Subsequently the desorbed H_2 is measured on a helium ionization or thermal conductivity chromatograph.

Helium containing 8 ppm of H_2 was used to simulate the gas to be sampled. Data points were taken with the helium ionization chromatograph for collection times of 1, 2 and 3 minutes. The following concentration factors were measured:

1 min = 12 X; 2 min = 21.6 X; 3 min = 28.1 X.

It should be pointed out that these were the first runs made with the sampler and that they were not replicated. However further data were obtained (up to six minutes of collection time) with the helium ionization chromatograph at a detector voltage of 300 V (necessitated by the large signal obtained);

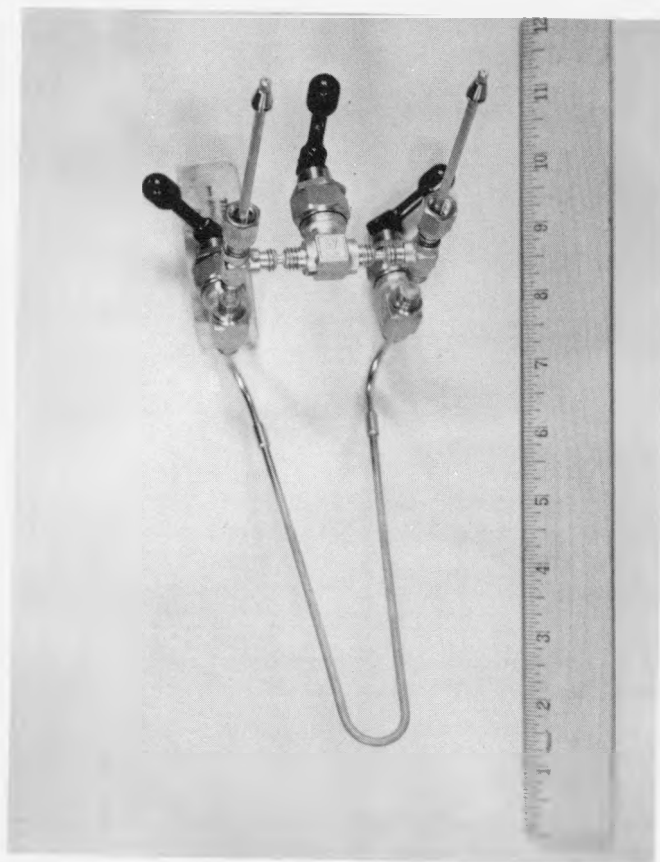


Fig. 5.3 Charcoal concentrating hydrogen sampler

but complete calibrations at 300 V have yet to be completed. Some brief experiments were also performed using a thermal conductivity gas chromatograph, and although the absolute calibration factors are not known at present, the trap did appear to exhibit a linear concentration behavior for collection periods up to four minutes. A second sampler was delivered to ORNL during this period for preparation and qualification for use in the tritium tests being conducted with the capsule.

5.6.3. High Pressure Water Saturator

Two high pressure water saturators (Fig. 5.4), to be used for the injection of water vapor at controlled levels into the helium gas streams of capsule GB-10 and the plateout and plugging loop, have been fabricated. The saturators were hydrostatically tested and found to be satisfactory to 15 MPa (2175 psig) and are being calibrated (i.e., P_{H_2O} vs. ice temperature and flow rate) and flow tested (i.e., pressure loss vs. flow rate and pressure level).

5.6.4. Planned Irradiation Capsule GB-11

A report evaluating the need for an additional sweep-gas capsule beyond capsule GB-10 was issued. The report recommends an immediate decision to include capsule GB-11 in funding plans for FY-77. A final decision on the need for capsule GB-11 will be made in the fiscal year transition quarter between normal FY-76 and FY-77. The decision will be based largely on the results of: (1) the laboratory simulation studies of the formation of constrictions in GCFR fuel rods by reaction of cesium with urania, (2) the effects of H_2 and H_2O impurities in helium in the coolant and the helium fission gas mixture in the fuel rod on the oxidation potential controlling cesium transport and the above reaction, and (3) possible fixes to the constriction problem. If the impurities in the helium have little or no effect on the cesium transport and the cesium-urania reaction, then the constriction fixes can be tested in the F-5 fuel rods, and GB-11 will not be needed for that purpose. Postirradiation examination must also confirm that the constriction developed in capsule GB-10 is the result of the

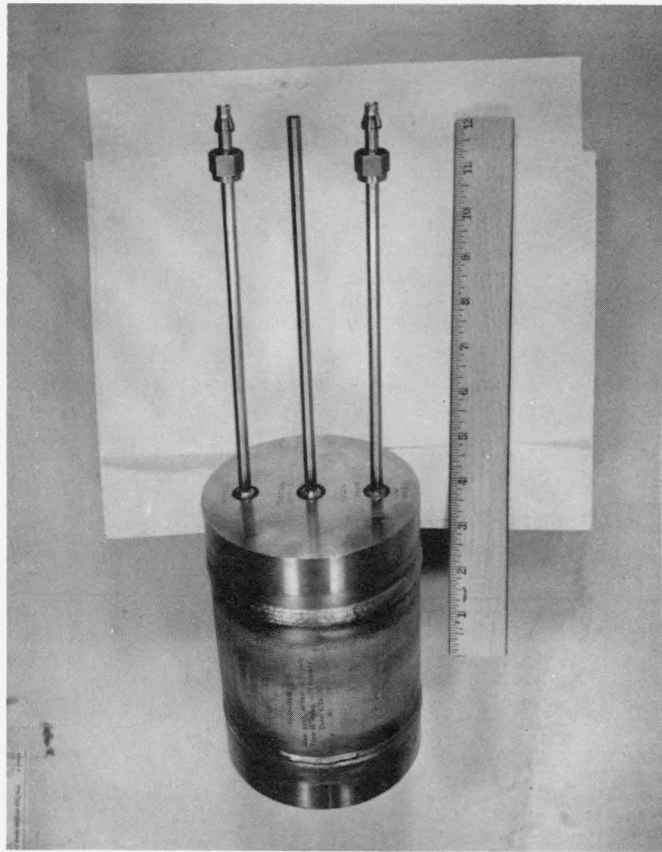


Fig. 5.4 High pressure water saturator

cesium-urania reaction, as assumed, before proceeding with constriction fixes in either or both the F-5 fuel rods or capsule GB-11.

5.7. HEDL CLADDING IRRADIATIONS

Inclusion of GCFR ribbed and smooth cladding specimens now being irradiated in a materials test experiment that was designed and fabricated by Hanford Engineering Development Laboratories (HEDL) was reported in a previous report.⁽⁶⁾

Seven cladding specimens irradiated in EBR-II to 3×10^{22} n/cm² are scheduled for de-encapsulation by HEDL in early November 1975. General Atomic's requirements on postirradiation testing have been forwarded to HEDL. The conditions most representative of the GCFR require a hoop to axial stress ratio of unity since the load to the cladding will be mainly from fuel cladding mechanical interaction rather than from fission gas pressure.

Fifteen additional cladding specimens ribbed by mechanical grinding have been prepared for inclusion in a materials test capsule being irradiated by HEDL in EBR-II. The cladding specimens were shipped to HEDL on October 4, 1975.

REFERENCES

1. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period November 1, 1974 through January 31, 1975," USERDA Report GA-A13379, General Atomic, March 1975.
2. Steichen, J. M., "High Strain Rate Tensile Properties of 20% C. W. Type 316 Stainless Steel," USERDA Report, HEDL-TME 74-39, Hanford Engineering Development Laboratory, 1974.
3. Paxton, M. M., "Mechanical Properties of Annealed and Cold Worked Type 316 Stainless Steel Fast Reactor Seamless Tubing," USERDA Report HEDL-TME 74-11, Hanford Engineering Development Laboratory, 1974.

4. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period May 1, 1975 through July 31, 1975," USERDA Report GA-A13565, General Atomic, October 1975.
5. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period February 1, 1974 through April 30, 1974," USAEC Report GA-A13021, General Atomic, June 1974.
6. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period August 1, 1974 through October 31, 1974," USAEC Report GA-A13328, General Atomic, December 1974.

VI. FUEL-ROD ENGINEERING (189a No. 13861)

The steady-state and transient performances of the fuel-, blanket-, and control-rods are being evaluated under this task to determine the performance characteristics, operating limits, and design criteria. Analytical tools (such as the LIFE-III code) are being adapted, updated and/or developed, and applied to the analysis of the rods and the evaluation of GCFR irradiation experiments. Continuous surveillance of the LMFBR fuels and materials development programs and technology is maintained to maximize the use of developing technology and material properties. Support is provided in the planning and designing of irradiation experiments.

6.1. FUEL-, BLANKET-, AND CONTROL-ROD ANALYSES

The objectives of this analytical task are to adapt and assess the ability of analytical behavioral models and computer codes (such as LIFE-III) to analyze the GCFR fuel-, blanket-, and control-rods and to evaluate irradiation experiments. The definition of design criteria, operating margins, failure criteria, and mechanisms under steady-state, transient, and load-following conditions will follow from these analyses.

6.1.1. Updating and Calibration of LIFE-III Code

Phase I of the post-calibration checkout of the LIFE code is nearly completed. The checkout revealed that some improvements to the code are desirable, for example, improved fuel and cladding swelling models, and the elimination of the discontinuity in the fuel/cladding friction coefficient, which causes long code running times during the late-in-the-life gap opening. Westinghouse-ARD and General Atomic are working on a solution for these problems. Phase II of the calibration has been planned and is underway. General Atomic's fuel rods G-1 and G-3 from the F-1 series irradiations

in EBR-II will be utilized in closing Phase I, since they fill in a power level, temperature, and burnup combination not yet checked out by the LIFE code.

It was unanimously agreed by the LIFE Code Working Group, that LIFE could be used as the basis for the development of a code for analysis of design basis transients. General Atomic's contribution and recommendations on this subject have been forwarded in a letter to GE where the documentation of the Working Group will be coordinated and sent on to RRD.

General Atomic has received from GE an update to the LIFE code, which permits the direct input of fuel rod cladding temperature at each axial section, as well as changes in power and flux profiles.

The updated listing of the calibrated LIFE Master code (October 1) has been obtained from the Berkeley CDC-7600 files, and work on the revisions necessary to obtain a gas-cooled version of the code has begun.

6.1.2. Control Rod Analytical Methods

The listing and a preliminary draft of the CONROD code was obtained during a recent trip to HEDL. This code is primarily a one-dimensional thermal hydraulics design code for the analysis of control rod assemblies. Changes to the code and to materials properties are necessary to make it applicable to the gas-cooled control rods in the GCFR.

6.1.3. Updating and Checkout of TAC-2D and LAMDA Codes

After updating and checkout of the LAMDA and TAC-2D codes, completed last quarter, the two codes have been documented and put on permanent files with an assigned custodian.

6.2. ANALYSIS OF IRRADIATION TESTS

The calculated cladding temperature for rods G-1 and G-3 have gone through a number of changes due to updating of the codes used in the power and temperature calculations (LAMDA and TAC-2D, respectively). The sequence of calculated cladding peak midwall temperatures to date for the highest power run of rods G-1 and G-3 are given in Table 6.1. Argonne National Laboratory-West has sent data that would change the shape of the axial power profile: a sharp end upturn of the curve, especially at the top end, is indicated after insertion of the shield in EBR-II for the runs following run no. 55. This change is now being scrutinized through communications with ANL-East and West and through plotting and analysis of zirconium counting measurements on cladding samples from these rods. The upturn seems to be real, although not nearly as sharp as indicated by ANL-W. It is anticipated that these updated profiles will raise the peak and outlet-end temperatures somewhat, but will only have major effects on the results for runs after run no. 55.

ANL-E has also been calculating cladding temperatures for the peak power run of rod G-1 using the HECTIC-III code and, as a new check, using the THTB code. These are also shown in Table 6.1 and are 739°C and 743°C (midwall) versus the GA calculated 737°C. The peak power run occurred prior to insertion of the reflector.

Both codes LAMDA and TAC-2D have been scrutinized and updated. With the exception noted below, it is believed that the latest results are a valid representation of the peak cladding midwall temperature for rods G-1 and G-3. The good agreement reached independently by ANL-E using two different codes appears to substantiate this confidence.

The only uncertainty that remains to be resolved is that of the power curve shape. This matter is now being evaluated for both cases prior to and after insertion of the shield in EBR-II (prior to and after run no. 55). If a change should result for the power shape also for these runs prior to

Table 6.1
CHRONOLOGY OF CLADDING TEMPERATURE CALCULATIONS FOR ROD G-1

Rod No.	Run No.	Peak Linear Power (kW/ft)	Peak Cladding Midwall Temperature (°C, Calculated)						
			Previous Reference		After Cladding Properties Updating	After TAC-2D and LAMDA Updating	After ^a Axial Power Shift Update	Calculations ANL-E	
			GA	ANL-E				HECTIC	THTB
G-1	51A	14.97	800	794	714	711	737	739	743
G-3	51A	14.39	722		652	647	670		
Data Source		ANL-E	(Ref. 1)	ANL-E				ANL-E	ANL-E
Date			8/1970	11/1974	3/19/75	4/24/75	8/4/75	8/21/75	8/21/75

^aAxial power shift = 7.62 cm (3 in.).

run no. 55, it will be in the direction of increasing the peak temperatures shown in the table by at most 5-10°C.

6.3. ROD CLADDING STRUCTURAL CRITERIA

Work is continuing on the draft version of the GCFR fuel blanket and control rod cladding structural criteria. Recently ERDA has established a National Working Group for design guidelines criteria for LMFBR core components. The purpose of the group is to assemble a unified set of core component structural criteria from the various existing criteria for use throughout the industry. General Atomic has been asked to join this effort. Consequently, the GCFR cladding structural criteria will become identical, or nearly identical, to the unified LMFBR criteria. The effort during this quarter has been to review the draft version of the GCFR structural criteria with relevant personnel and obtain comments and suggestions. Most of these suggestions will be presented at the forthcoming (December 9-10, 1975) meeting of the National Working Group. In preparation for this meeting, the LMFBR material to be discussed is being reviewed, and comments are being prepared.

6.4. ROD ANALYSIS AND PERFORMANCE

6.4.1. Blanket Rod Analysis

Large diameter pellets (12 to 25 mm) are being considered for use in the radial blanket fuel rods. This would increase the fertile material volume fraction with a consequent increase in breeding ratio, a decrease in fabrication costs, and a reduction in shielding requirements. The experience gained by the Canadians (AECL), the English (SGHWR), and Westinghouse on irradiation and performance of large diameter fuel pellets was reviewed and analyzed for application to GCFR blanket rods. The above experience was gained primarily for Zircaloy clad UO_2 in light and heavy water cooled reactors and experiments. The Westinghouse experience included also ThO_2/UO_2 in both Zircaloy and stainless steel cladding.

The conclusion from the total experience, which encompassed burnups in excess of 10,000 MWd/MTM and linear heating rates between 270 and 1100 W/cm (8.3 to 34 kW/ft), is that large diameter pellets (12 to 25 mm) could be utilized for the radial blanket rods in the GCFR system. No performance handicap is foreseen as a result of the size increase at the power levels and burnup predicted for the radial blanket rods (30 to 525 W/cm) and maximum burnup of 5,000 MWd/MTM. It was recommended that pellets with dished ends and edge chamfering be utilized to minimize axial expansion and local stress concentrations.

The four and six year management schemes for the radial blanket rods utilizing 25.4 and 20.3 mm diameter pellets were set up and run using the LIFE code, modified so as to result in a very low end-of-life internal pressure 0.193 MPa ($\sim$ 28 psi) and less than 10% helium fill gas dilution by fission gases (simulating vented rods). The results are now being analyzed. A preliminary examination indicates that no end-of-life mechanical strains are predicted for fuel-cladding diametral gaps in the range of 0.15 to 0.18 mm for either of the management schemes or pellet diameters.

6.4.2. Fuel-Rod Cladding Stress Analysis

A study of creep collapse of the GCFR fuel-rod cladding began during this reporting period. Because of the pressure equalization system (PES), a failure of the fuel rod by creep collapse under external pressure is not anticipated. However, should a rod be inadvertently sealed during manufacture, the initial operation at elevated temperatures would result in creep deformation in a collapse mode. Only after sufficient fission gas pressure has built up, would this trend reverse.

The initial analysis used the computer code COLLAPSE, which analyzes a one-quarter section of tube using finite difference techniques. The section was modeled by 25 wedge shaped segments of 10 layers each. The output consists of a midwall displacement and the axial and hoop stresses at each of the 10 layers for all segments. The current irradiation and thermal creep

of 20% cold worked 316 SS were incorporated into the code. No account was taken of the fuel pellets as a supporting structure nor of the linearly increasing fission gas pressure, which reduces the collapse mechanism.

The initial out-of-roundness was taken to be ± 0.051 mm:

$$D_{\max} = D_{\text{nominal}} + 0.051 \quad D_{\min} = D_{\text{nominal}} - 0.051$$

Wall thicknesses of 0.502 mm and 0.381 mm were investigated. The applied external pressure was 8.9977 MPa. The collapse times were found to be 17,600 hrs and 2,200 hrs for the two thicknesses.

The analysis will continue by computing the fission gas buildup to determine if collapse will actually occur. If it does not, then the time-to-burst from internal pressure will be calculated.

The LIFE code is being applied to the analysis of the simulated GCFR sealed rods (assuming that the vent hole of the peak rod, operating at peak and 15% overpower, is sealed from beginning of life due to a manufacturing defect). Four cases have been set up representing a combination of peak and 15% overpower with cladding thicknesses of 0.38 and 0.51 mm (15 and 20 mils).

The status of the reference fuel steady-state irradiation program was reviewed, and a program plan for completion of the Reference Fuel National Program was drafted at the Reference Fuel Irradiation Test Program Review Meeting at ERDA/RRD which GA attended.

REFERENCE

1. Flynn, P. W., et al, "High Temperature Fast Flux Irradiation Program for Mixed Oxide Fuel Rods," USAEC Report GA-A10264, General Atomic, October 1970.

VII. NUCLEAR ANALYSIS AND REACTOR PHYSICS (189a No. SU008)

The scope of activities planned under this subtask encompasses the validation and verification of the nuclear design methods that will be applied to the GCFR core design. This will be done primarily by direct evaluation of the methods with a critical experiment program specifically directed toward GCFR development. Program planning and coordination activities, critical assembly design and analysis, and the necessary methods development will be carried out during the course of this program.

In the previous quarterly period, the experimental program for the Phase I core was completed, and the loading for the Phase II core commenced. Approval for the Phase II experimental program was granted by ERDA RRD in May. Additional benchmark analyses of ZPR-6, Assemblies 6A and 7, were performed to complete the Cross-Section Evaluation Working Group data testing participation activities for ENDF/B-IV nuclear data.

On September 16 at ANL, the Phase II core went critical with a critical radius close to that predicted by GA and ANL. Planning was completed jointly with ANL for a revised steam entry experiment to be performed in this phase. Post-analysis of the Phase I core was carried out with ENDF/B-IV nuclear data for criticality, polyethylene zone worth, central reactivity coefficients, and central reaction rate ratios in the as-built core configuration. Detailed specifications for an improved spectrum code were issued.

7.1. PHASE I CRITICAL ASSEMBLY

7.1.1. Post Analysis of Phase I GCFR Critical Experiments

Criticality calculations for the as-built experimental configuration of the Phase I GCFR assembly on ZPR-9 were completed. The two-dimensional

diffusion theory program, ADGAUGE, was utilized with 10-group cross sections derived from ENDF/B-IV. Heterogeneity effects in the plate-type critical assembly were taken into account by using the two-region option in the resonance treatment by GGC-5 and cell-plate flux advantage factors obtained from a DTFX slab calculation. Investigations of the streaming effect on reactivity were carried out with the directional diffusion coefficient capability in ADGAUGE.

For the experimental just-critical core radius of 90.60 cm, the post-analysis with ENDF/B-IV resulted in a k of 0.99863 with all heterogeneity effects and streaming corrections included. Without the streaming correction, i.e., with no directional diffusion modifiers used in ADGAUGE, an eigenvalue of 1.01912 was obtained, showing a streaming correction of -2.01% $\Delta k/k$. The corresponding calculations from the preanalysis using ENDF/B-III, for a radius of 90.58 cm, gave a $k = 1.0005$. Thus, the conversion to the newer data file has resulted in a calculated reactivity drop of about 0.2%.

7.1.2. Analysis of "Steam-flooding" Experiments

As a preliminary indication of the possible effects from steam ingress into a GCFR core, a series of simulated steam-entry experiments was conducted in a small central region of the Phase I assembly on ZPR-9. Polyethylene foam slabs were inserted into the region void channels, amounting to about 47 volume percent of the drawer loadings. The foam density of 0.035 g/cm^3 CH_2 effectively simulated steam in the channels at about 0.045 g/cm^3 . The region consisted of the central 5 x 5 drawer block and extended to 30.5 cm (12 in.) in both assembly halves for a total region volume of 46.62 liters, about 1.5% of the full core volume.

Based on the preanalysis at GA and ANL for Phase I using cross-section sets derived from ENDF/B-III, a small negative reactivity signal was anticipated from filling the region voids with the CH_2 foam. The measurement, however, yielded a positive reactivity of about 47 cents. Consequently,

the experiments were extended to include other densities of simulated steam entry. For halving the foam density, two measurements were carried out: one with the foam inserted in only half of the void channels, the other with the foam slabs in all the channels punched with holes to achieve approximately half density, or $0.0175 \text{ g/cm}^3 \text{ CH}_2$ slabs. A quarter-density experiment also was done using the half-density slabs in half of the channels. Table 7.1 lists the total CH_2 masses inserted in the four experiments and the resulting reactivity effects.

As seen in the table, the ENDF/B-IV analysis gives CH_2 worths in the center zone in satisfactory agreement with measurements. The significant change from the ENDF/B-III preanalysis prediction indicates the sensitivity of moderator reactivity effects to changes in the basic core-material cross sections. The changes in sign and magnitude are probably due mostly to the lowering of U-238 capture data in ENDF/B-IV.

7.1.3. Transport Calculation of Phase I

Real and adjoint P_1S_4 transport calculations for the GCFR Phase I as-built core were run with TWOTRAN. The standard GA 10-group structure and a 28×25 mesh point grid were utilized. These corresponded exactly to an existing 2-D ADGAUGE diffusion calculation so that the transport correction could be directly determined. Table 7.2 exhibits the calculations, which were performed for a configuration with isotropic neutron transport (i.e., no directional diffusion approximations). The effect of finer spatial mesh was investigated and was found to have little effect on eigenvalue in the diffusion calculations. It is seen that for comparable convergence criteria, the transport correction for the Phase I core is $\Delta\rho = 7.3 \times 10^{-3}$. This correction is larger than that found for ZPR6-7 but comparable to ZPR3-48, as ascertained in a study of LMFBR critical assemblies.

7.2. PHASE II CRITICAL ASSEMBLY

7.2.1. Phase II Critical Mass

The Phase II core went critical at ANL on September 16, 1975, with

Table 7.1

SUMMARY OF "STEAM-FLOODING" CALCULATIONS FOR 5 X 5 DRAWER CENTRAL
ZONE OF PHASE I GCFR CRITICAL ASSEMBLY

Central Zone (46.62 l) CH ₂ Content		Measured Worth (Ih [inhours])	Δk/k Calculations with ADGAUGE			
			Pre-Analysis with ENDF/B-III (Worth, Ih, assuming 10 ³ Ih/%)		Post-Analysis with ENDF/B-IV (Worth, Ih, assuming 10 ³ Ih/%)	
Void- Channel Density (g/cm ³)	Total CH ₂ (g) C = Calculation M = Measurement		Isotropic Diffusion	With Streaming Effects	Isotropic Diffusion	With Streaming Effects
0.00875	C 192.17	26.107 ± 2.87			13.8	9.0
	M 194.4					
0.0175	M 373.80	59.87 ± 3.28			43.5	49.3
	C 384.35					
	M 408.20	68.99 ± 2.72				
	C 768.70					
0.0350	M 784.0	157.21 ± 2.60	-33	-18.3	148.6	160.8

Table 7.2
EIGENVALUE COMPARISONS
(ENDF/B-IV Data)

Code	2-D Diffusion (ADGAUGE)	2-D Diffusion (ADGAUGE)	2-D Transport (TWOTRAN)	2-D Transport (TWOTRAN)
Mesh	38 x 31	28 x 25	28 x 25	28 x 25
P_{lS_N}	---	---	P_{lS_4}	P_{lS_8}
k_{eff}				
$\epsilon = 10^{-3}$	---	---	1.029750	1.028874
$\epsilon = 10^{-4}$	---	---	1.027838	---
$\epsilon = 10^{-5}$	1.019122	1.020007	1.027671	---
k_{eff}^* (adjoint)				
$\epsilon = 10^{-3}$	---	---	1.026929	---
$\epsilon = 10^{-4}$	---	---	1.027474	---
$\epsilon = 10^{-5}$	1.019051	1.019712	1.027633	---

a measured excess reactivity of 113 lh. Adjustments to the critical radius, made in order to derive the "zero-excess" model corresponding to an R-Z calculation, resulted in a final fissile plutonium loading of 619.1 kg and a critical radius of 580.6 mm. The GA calculation had predicted an expected critical radius of 590.0 mm.

7.2.2. Analysis of Phase II "Steam-flooding" Experiment

An extensive series of calculations has been made using the 2-D ADGAUGE code to define a series of polyethylene insertion experiments. A plan was devised that will exhibit the effects of varying steam density and the change in steam worth with boron insertion.

7.3. METHODS DEVELOPMENT

7.3.1. Spectrum Code Improvement

Specifications were developed for features to be included in a revised spectrum code to reflect "state-of-the-art" advances. Improved theoretical treatments in the resonance energy region as well as elimination of known inadequacies have been addressed. Improved formats and edit information have been specified.

A trip to ANL-Applied Physics Division was made in order to compare spectrum codes, discuss improvements, and examine discrepancies, specifically the diffusion coefficient discrepancy existing between GA and ANL codes.

VIII SHIELDING PHYSICS (189a No. SU008)

The purpose of the shielding task is to verify the adequacy of the methods and data for the design of GCFR shields and to evaluate the effectiveness of various shield configurations. In addition, this task will coordinate and provide liaison with the analytical and experimental shielding activities at the Oak Ridge National Laboratory (ORNL).

In the last quarterly period, analyses of the lower and upper axial shields were made. Gamma-ray heating calculations were also made, and a topical report on the shielding analyses completed in FY-75 was prepared and issued.

During this quarter, a shielding benchmark calculation was made in coordination with ORNL in order to have a consistent basis for comparison of GA and ORNL data and methods. Conversion from ENDF/B-Version III to IV was made and checked. The effects of removing the third row of the radial blanket and the effect of fissile buildup in the radial blanket on the shielding requirements were studied.

8.1. PHYSICS ANALYSIS

8.1.1. Gamma-ray Heating Benchmark Calculations

A complete core, blanket, shield, 39-group, neutron-coupled gamma-ray heating calculation was performed for the 300-MW(e) GCFR and used as a benchmark for comparison with results from ORNL. ENDF/B-III neutron-coupled gamma-ray cross section sets generated previously for ANISN calculations^(1,2) were used. Calculations were performed in one-dimensional, cylindrical geometry with S_6 angular quadrature and P_3 anisotropic scattering. As reported previously,⁽¹⁾ the 39-group coupled set

consists of 24 neutron energy groups (including two thermal groups) and 15 gamma-ray energy groups.

The shield configuration used for this benchmark study was the first "reduced" boronated shield investigated and reported in Refs. 2 and 3, except that a one-centimeter steel cladding was added to the carbon of the inner shield as shown in Fig. 14 of Ref. 2 and in Fig. 8.1 of this report. This shield is referred to as "Boronated Shield No. 1".

The neutron fluxes obtained in the present analysis are given in Fig. 8.2. The energy groups shown are for neutrons with energy, $E > 0.9$ MeV; $E > 0.086$ MeV; $E < 2.38$ eV (thermal); and total. The energy group boundaries of 0.9 MeV and 0.086 MeV correspond to the lower energies of the ORNL energy groups.

The corresponding gamma-ray heating results are given in Fig. 8.3, with heating in W/cm^3 versus radius in cm. Comparison with the gamma-ray heating results reported in Fig. 14 of Ref. 2 shows that in the blanket and inner shield, the present results are about 30% lower; but in the outer shield, liner, and concrete, the results are in very good agreement. It should also be noted that the results of Ref. 2 were obtained at GA with the ANISN code, using a shell source at the inner radius of the blanket.

8.1.2. Comparison with ORNL Calculations.

Comparison of the GA benchmark results with ORNL calculations should be considered preliminary. The ORNL results were calculated with the ANISN program using neutron-coupled gamma-ray cross-section sets of 51 neutron energy groups (including four thermal groups with $E < 2.38$ eV) and 25 gamma-ray energy groups. The nuclide densities, materials, fine mesh intervals, etc. were specified by GA. The intent of the benchmark was to reduce any differences between GA and ORNL calculations to differences in cross sections and group structure.

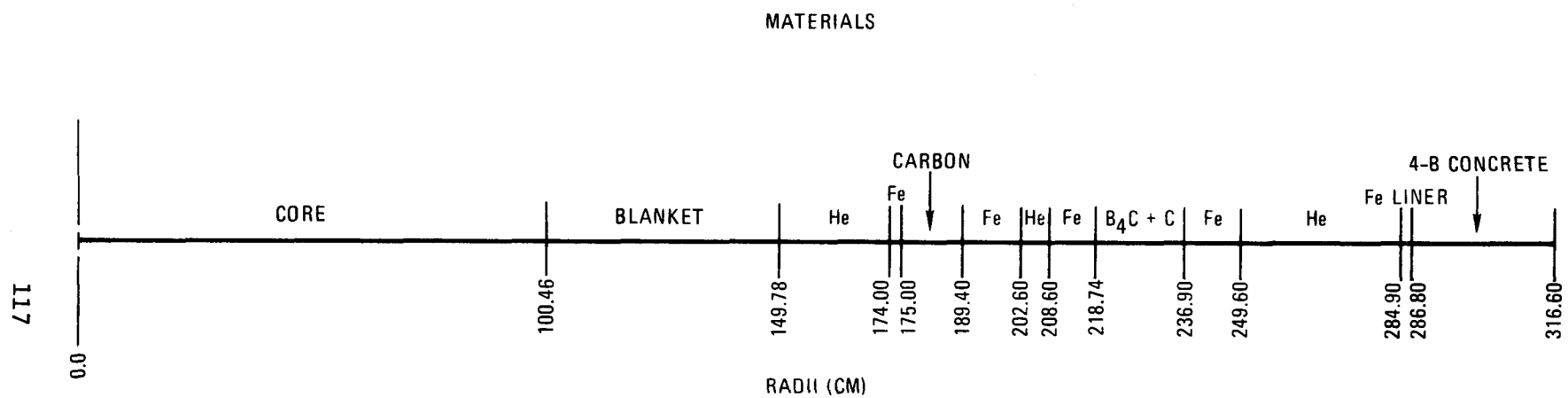


Fig. 8.1 Geometry for neutron-coupled, gamma-ray transport benchmark calculations

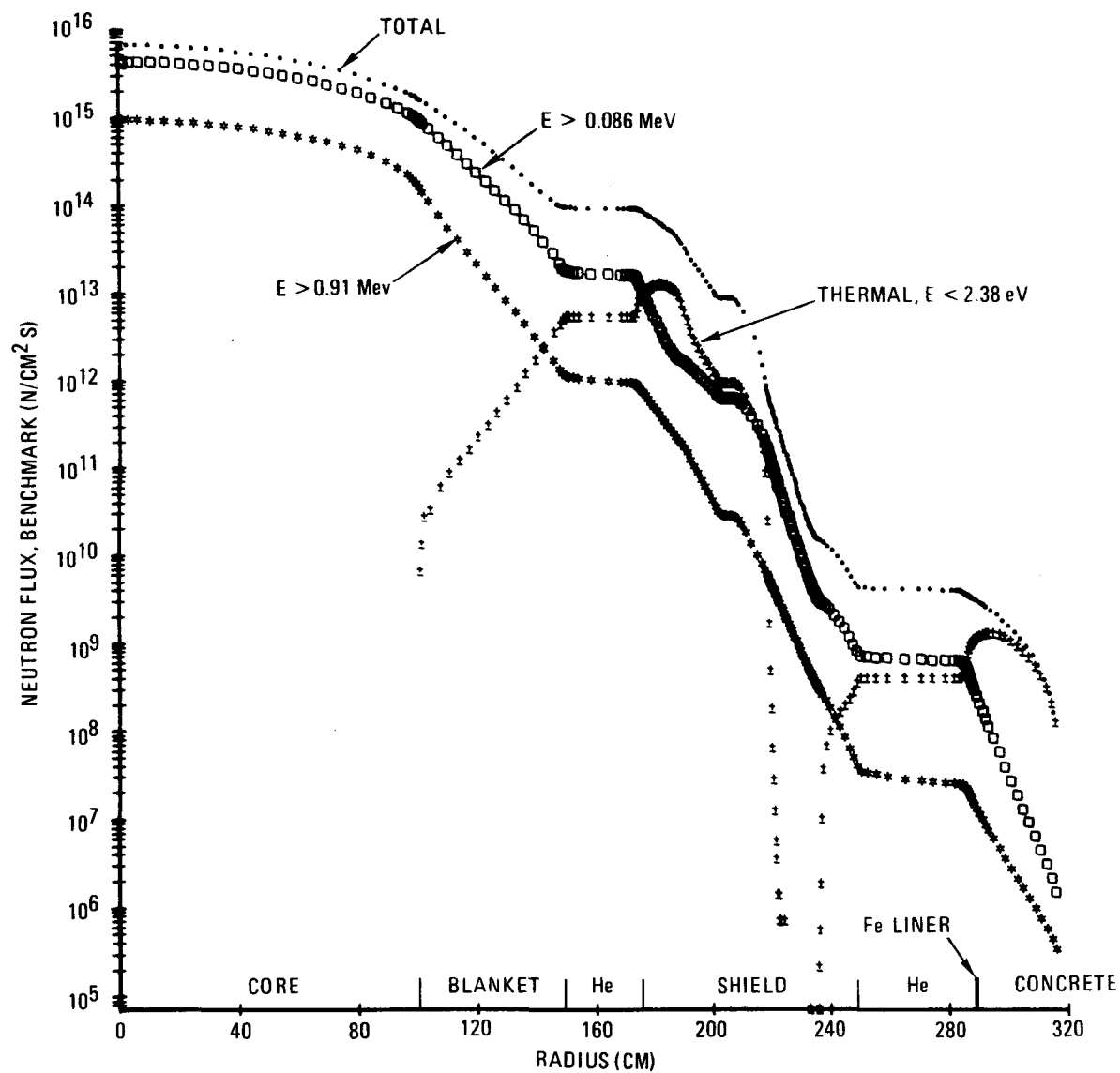


Fig. 8.2 Results of 300-MW(e) GCFR shield benchmark calculation

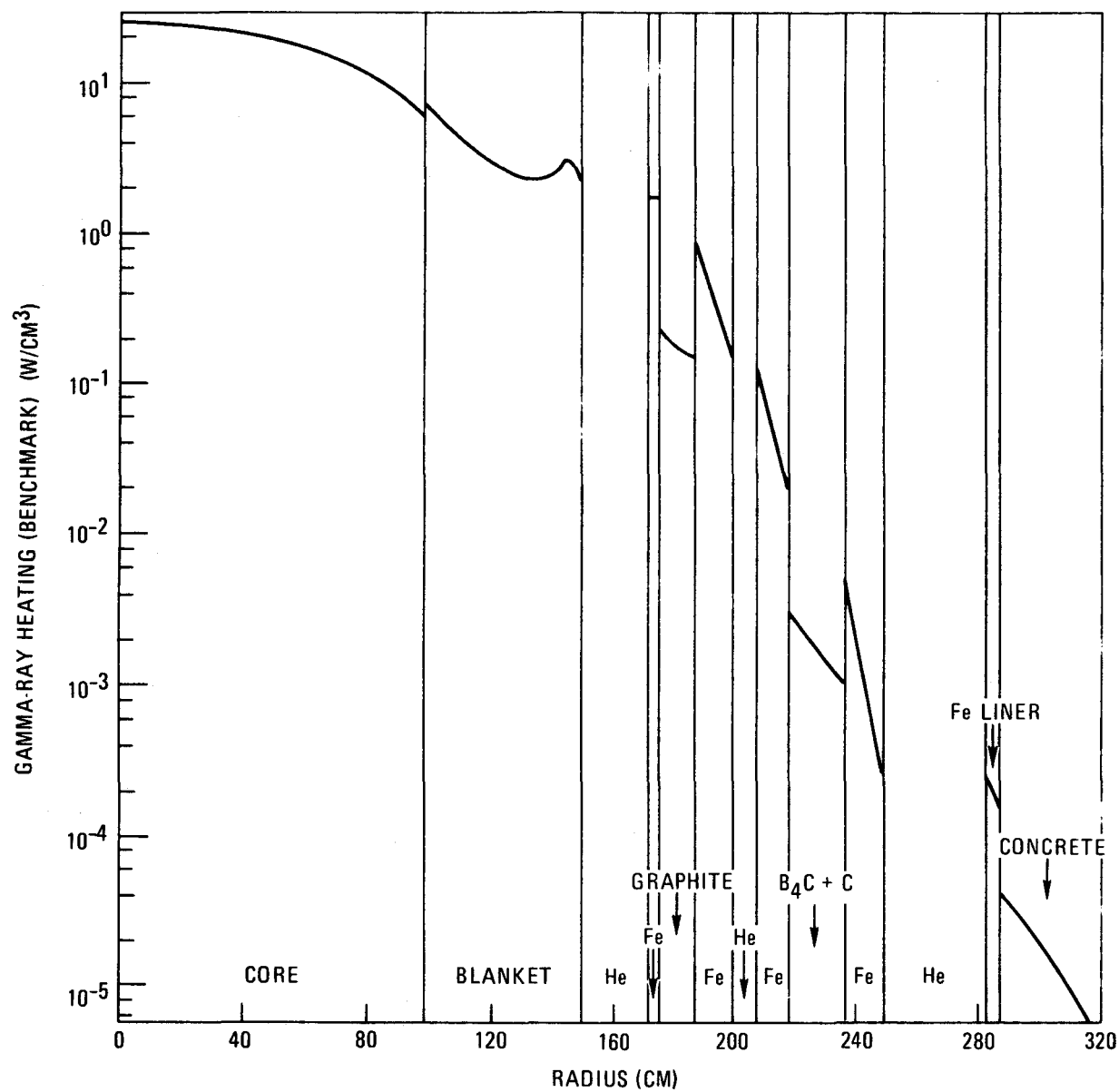


Fig. 8.3 GCFR benchmark gamma-ray heating for core, blanket, shield, liner, and concrete

The results are compared in Figs. 8.4 and 8.5. In Fig. 8.4, the ratios of neutron fluxes from the ORNL results to GA results are plotted versus radius, for neutrons with $E > 0.9$ MeV, $E > 0.086$ MeV, $E < 2.38$ eV, and total flux. In Fig. 8.5 the gamma-ray heating in W/cm^3 versus radius in the blanket, shield, liner, and concrete is plotted for both GA and ORNL calculations.

Since these results are preliminary, a more complete analysis and discussion will be given later. Nevertheless, from the fast flux results in Fig. 8.4 for $E > 0.9$ MeV, the good agreement throughout a nine-order-of-magnitude change of the flux indicates that the overall methods and cross sections are substantially the same. The differences indicated in the remainder of the graphs can tentatively be associated with differences in cross-section weighting and numbers of energy groups. A more complete analysis of the results will be made after discussions with ORNL.

8.2. GENERATION OF ENDF/B-IV CROSS SECTIONS FOR GCFR SHIELDING

Generation of updated shielding cross section sets utilizing ENDF/B-IV data was completed, and a benchmark calculation comparison with ENDF/B-III data was performed for the integral core, three-row blanket, and reference shield configuration shown in Table 8.1. The calculation was performed with the LDFX code in cylindrical geometry with 24 groups, P_3 anisotropic scattering, and S_6 angular quadrature. Calculations using ENDF/B-IV data resulted in about 20% lower ($E > 0.1$ MeV) flux levels throughout the radial shield and at the PCRV liner. The thermal flux and the total flux were 60% lower at the PCRV liner. The differences were attributed to revisions of the carbon, oxygen, U-238 and stainless steel cross sections.

8.3. RADIAL SHIELD DESIGN STUDIES WITH A TWO-ROW RADIAL BLANKET

The primary effort during this quarterly period was the determination of the radial shield requirements with a revised two-row radial blanket

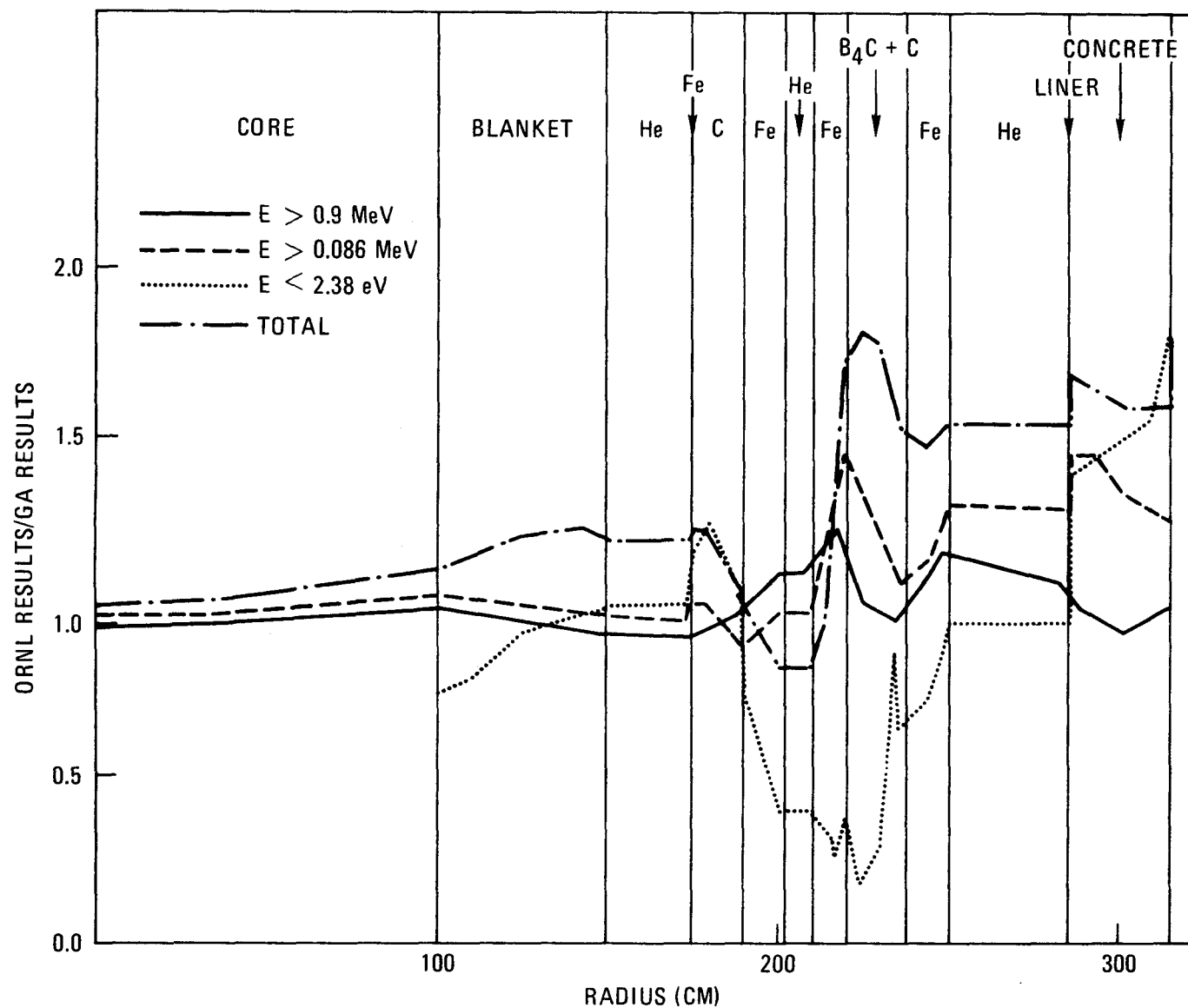


Fig. 8.4 Comparison of ORNL and GA GCFR benchmark results - ratios of neutron flux

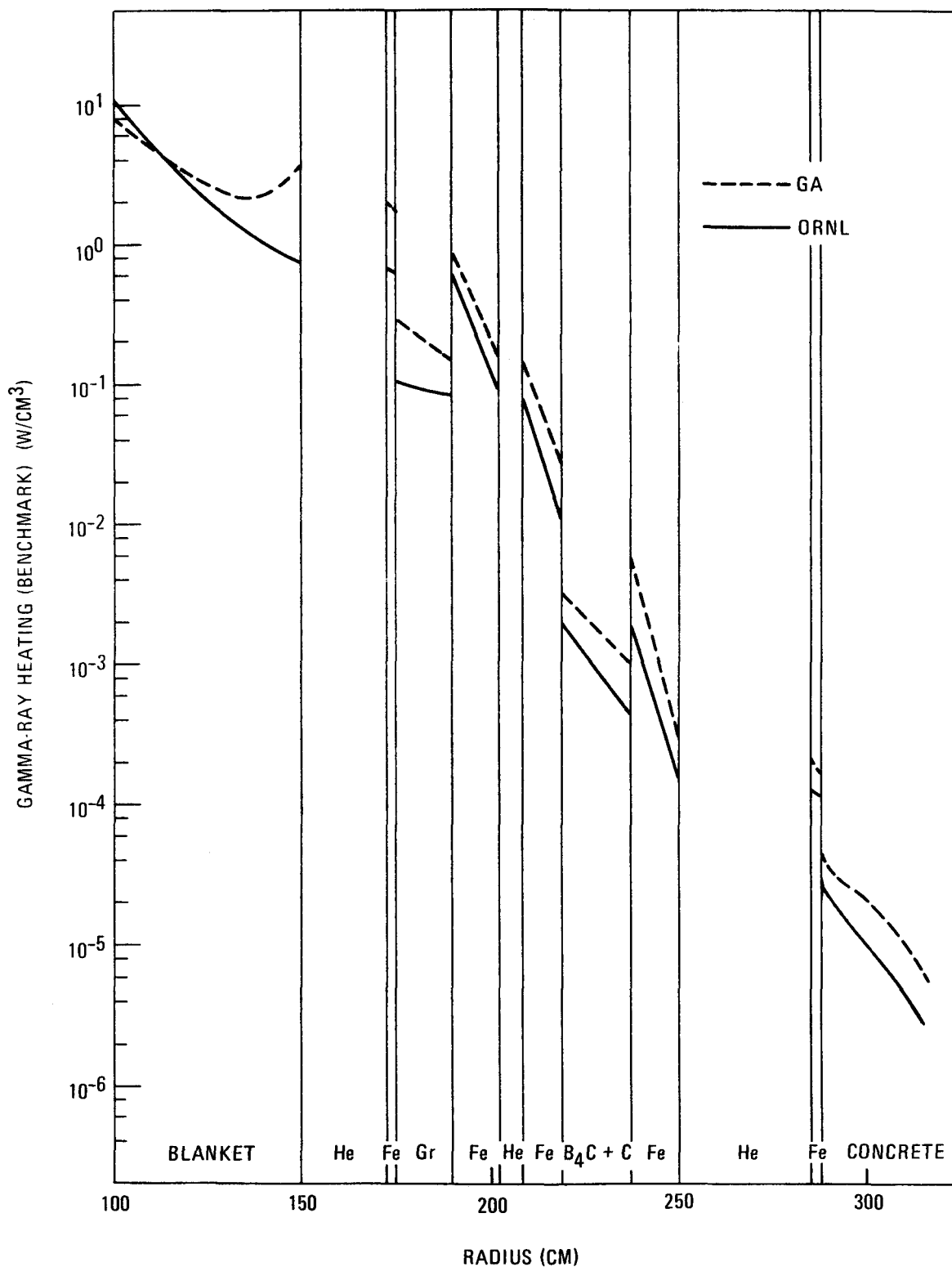


Fig. 8.5 GCFR benchmark gamma-ray heating for blanket, shield, liner, and concrete, comparing GA and ORNL results

Table 8.1
GEOMETRY FOR THE REFERENCE RADIAL SHIELD

Material	Distance from Core Center (cm)	Thickness (cm)
Core	0 - 100.46	100.46
Blanket	100.46 - 149.78	49.32
He	149.78 - 174.0	24.22
C	174.0 - 189.4	15.4
SS 304	189.4 - 202.6	13.2
He	202.6 - 208.6	6.0
SS 304	208.6 - 218.7	10.1
C	218.7 - 236.9	18.2
SS 304	236.9 - 249.6	12.7
C	249.6 - 267.4	17.8
SS 304	267.4 - 280.2	12.8
He	280.2 - 315.5	35.3
Fe	315.5 - 317.4	1.9
4-B concrete	317.4 -	

design which is currently being studied for possible inclusion in the demonstration plant reference design. The effects of core enrichment zoning and of radial blanket fissile buildup for various blanket management schemes were included in the analyses.

Tentative design criteria determined from structural-mechanical considerations were established and energy-dependent damage functions, which account for the total material damage due to the entire neutron spectrum, were used to establish fluence limits based upon the allowable material property changes.

The revised two-row blanket incorporates larger rods with a smaller lattice pitch-to-diameter ratio than the reference three-row blanket. The results of a slab geometry calculation summarized in Table 8.2 shows

Table 8.2
FLUX REDUCTION IN THE REFERENCE THREE-ROW BLANKET AND
THE REVISED TWO-ROW BLANKET

Blanket	ThO ₂ Volume Fraction	Blanket Thickness (cm)	Flux Reduction in Blanket		
			E > 1.0 MeV	E > 0.1 MeV	Total
Reference Three-row	0.513	49.32	65	42	32
Revised Two-row	0.595	33.23	33	24	19

that although the two-row blanket has a larger ThO₂ volume fraction, its net opacity is less, and the emergent fast, E > 0.1 MeV, and total flux is about a factor of two higher. As a result, fluence levels and heating rates in the shielding will increase.

8.4. SHIELDING DESIGN CRITERIA

The following criteria, based upon structural-mechanical and thermal-hydraulic considerations, have been tentatively adopted to establish neutron fluence and heating rate limits:

1. The total fluence for permanent, non-replaceable type 304 stainless steel shielding components shall not exceed the value that gives a residual total elongation of 10%.*
2. The fluence for graphite shield components shall not exceed 3×10^{22} for E > 0.1 MeV.
3. The total fluence at the PCRV liner shall not exceed the value that gives a 75°C shift in the nil ductility temperature.

* It is expected that rigorous structural analysis and an improved mechanical design of the inner radial shield will allow significant relaxation of this criterion.

4. The heating rate in the PCRVR shall not exceed 2 mW/cm^3 .
5. The gamma dose rate to the innermost PCRVR tendon lubricant shall not exceed 5×10^8 rads.
6. Temperature limits for steel and graphite, which are related to fluence limits, shall not be exceeded.

8.4.1. Fluence Limit Calculation

An energy-dependent damage function was used to establish the fluence required to attain the limiting material property change specified by the design criterion. The total material property change P is evaluated from the following relation:

$$P = \phi t \int G^P(E) dP(E) dE , \quad (1)$$

where ϕt is the total neutron fluence required in the neutron spectrum $\phi(E)$ to attain the property change P ; $\phi(E)$ is the neutron spectrum normalized to a unit flux, that is,

$$\int \phi(E) dE = 1.0 , \quad (2)$$

and $G^P(E)$ is the energy-dependent damage function derived specifically for the limiting material property change. In multigroup form with G broad groups, Eq. (1) is written

$$P = \phi t \sum_{g=1}^G \bar{G}_g^P \phi_g , \quad (3)$$

where $\bar{G}_g^P$ is the average damage function in the g^{th} broad group, and ϕ_g is the group g integral flux normalized such that

$$\sum_{g=1}^G \phi_g = 1.0 \quad . \quad (4)$$

From Eq. (3) the total fluence limit is

$$\phi t(\text{Total}) = P \left[\sum_{g=1}^G \bar{G}_g^P \phi_g \right]^{-1} , \quad (5)$$

and the effective $E > 0.1$ MeV fluence limit, which still accounts for the total neutron spectrum damage, is

$$\phi t(E > 0.1 \text{ MeV}) = \phi t(\text{Total}) \times \frac{\phi(E > 0.1 \text{ MeV})}{\phi(\text{Total})} \quad . \quad (6)$$

8.4.2. Neutron Damage Calculation

Eigenvalue calculations were performed for each of the five radial shield configurations shown in Fig. 8.6. The radial traverse flux was computed at the core midplane using the IDFX code⁽⁴⁾ with 24 groups, P_3 anisotropic scattering, and S_6 angular quadrature. The cross section sets were obtained from the ENDF/B-IV data. A calculation was performed for each shielding configuration for a three-region core with each region corresponding to an enrichment zone and for a single region blanket with the beginning of life (BOL) thorium nuclide density and, therefore, no fissile buildup. To determine the effect of core zoning on shielding component fluence levels, the configuration 1 calculation was repeated, considering a homogenized single region core with the BOL blanket. The effect of blanket fissile buildup on the shielding component fluence levels was determined by repeating configuration 1 for the three-region core

(DISTANCES FROM CORE CENTER (CM) ARE GIVEN BELOW EACH FIGURE)

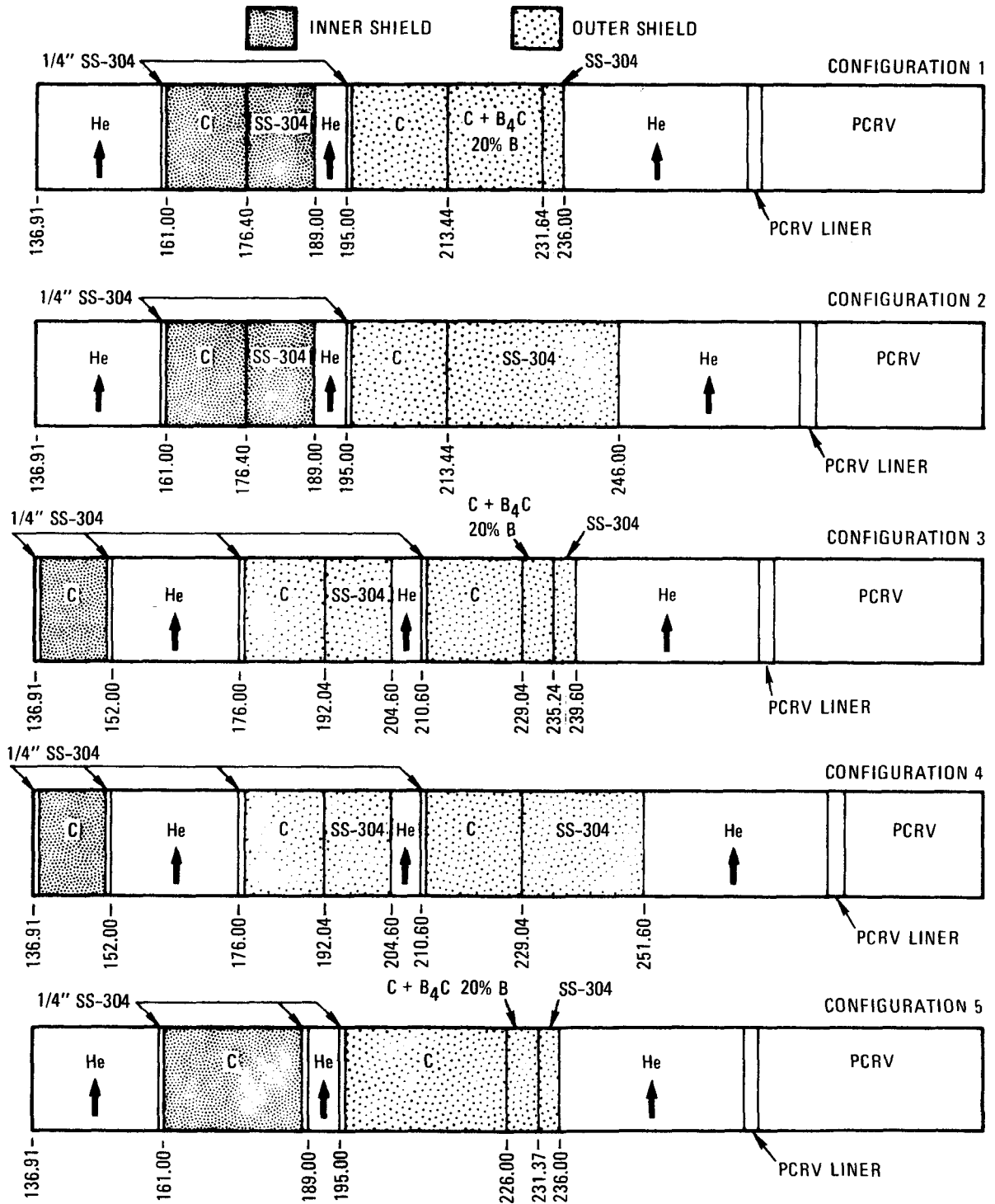


Fig. 8.6 Geometries for radial shield configurations

together with a two-region blanket, with each homogenized region corresponding to a blanket row and including the end of blanket management cycle six (for a six-cycle blanket management scheme) fissile inventory. At the end of the sixth cycle, the first and second blanket rows have been exposed for three years and six years, respectively, which represents a case of maximum blanket power production. Each core-blanket model is depicted in Fig. 8.7.

Table 8.3 gives the fluence level and the fluence limit at the innermost stainless steel region of the inner radial shield for each shield configuration. The results are identical for configurations 1, 2 and 5. A comparison of cases 1A and 1C shows the effect of the end of cycle-six blanket fissile buildup on the fluence limit and the fluence level. The $E > 0.1$ MeV flux emerging from the blanket more than doubles, from 4.1×10^{13} to 8.9×10^{13} . As a result of this significantly harder spectrum, the total fluence limit decreases from 4.4×10^{22} to 2.8×10^{22} . For the BOL blanket case 1A, 22% of the total damage response is due to the $E > 1.0$ MeV flux, and 71% of the damage response is due to the $E > 0.1$ MeV flux. When the end of cycle-six blanket power production is included (case 1C), the damage contribution of the $E > 1.0$ MeV flux and the $E > 0.1$ MeV flux increase to 41% and 84% respectively.

The details of the fluence limit calculations for cases 1A and 1B are given in Tables 8.4 and 8.5 respectively. The damage function used for the calculation⁽⁵⁾ is for type 304 stainless steel irradiated at 371°C (700°F). Note the shift of the spectrum for case 1C to the higher energy groups that have the larger response functions. Comparing cases 1A and 1B it is seen that the effect of the core enrichment zoning is to slightly decrease the total fluence limit and to increase the $E > 0.1$ MeV flux emerging from the blanket by about 40%. The predominant effect of the core zoning is a flatter core power distribution, which increases the flux at the core blanket interface and throughout the shield. The core zoning only slightly hardens the spectrum at the inner shield. The blanket power production due to fissile buildup results in a significantly

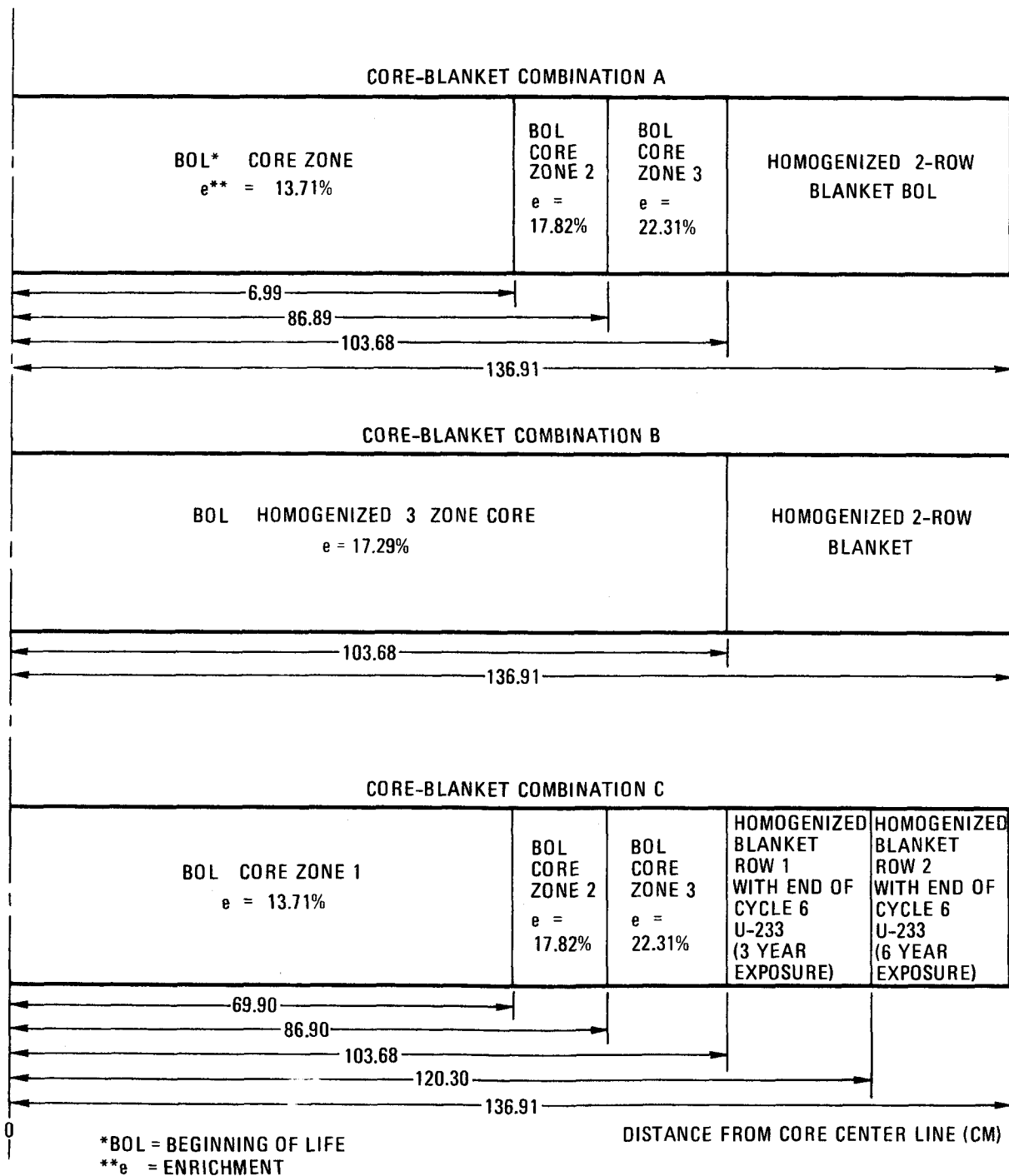


Fig. 8.7 Core-blanket combinations

Table 8.3

FLUENCE LEVEL^a AND LIMIT AT INNERMOST STAINLESS STEEL REGION OF THE INNER RADIAL SHIELD FOR EACH RADIAL SHIELD CONFIGURATION
(Based on Fluence Limit to Attain 10% Residual Total Elongation in Type 304 Stainless Steel)

Case		Total Fluence Limit (n/cm ²)	Total Fluence (30 years/ 80% load factor) (n/cm ²)	Effective E > 0.1 MeV Fluence Limit (n/cm ²)	Fast Fluence E > 0.1 MeV (30 years/ 80% load factor) (n/cm ²)	Time, Inner Radial Shield to Fluence Limit (years/80% Load Factor)	Percent of Damage Response Due to E > 1.0 MeV Flux	Percent of Damage Response Due to E > 0.1 MeV Flux	Percent of Damage Response Due to 2.38 eV < E < 0.1 MeV Flux	Percent of Damage Response Due to E < 2.38 eV Flux
Radial Shield Configu- ration	Core- Blanket Combi- nation									
1,2 & 5	A	4.4 + 22	1.3 + 23	1.1 + 22	3.1 + 22	10.25	22	71	25	4
1,2 & 5	B	4.6 + 22	9.4 + 22	1.1 + 22	2.2 + 22	14.60	21	70	26	4
1,2 & 5	C	2.8 + 22	2.0 + 23	9.6 + 21	6.7 + 22	4.25	41	84	14	2
3 and 4	A	4.1 + 22	1.6 + 23	1.1 + 22	4.1 + 22	7.75	22	72	24	4

^aDivide fluence by 7.6×10^8 seconds to obtain flux

Table 8.4

CALCULATION OF FLUENCE REQUIRED TO ATTAIN 10% RESIDUAL TOTAL ELONGATION
IN TYPE 304 STAINLESS STEEL AT THE INNERMOST REGION OF THE INNER
RADIAL SHIELD FOR RADIAL SHIELD CONFIGURATIONS 1, 2 AND
5 AND CORE-BLANKET CONFIGURATION A

Broad Group g	Lower Energy of Group g (MeV)	Normalized Group Flux ϕ_g^a [(n/cm ² .sec) x 10 ²²]	Nominal Group Response Function $\bar{G}_g^{Pa}$ [%/(n/cm ²)]	Nominal Group Response $\bar{G}_g^P \times \phi_g^b$ [%/(n/cm ²) x 10 ²³]
1	4.97	0.085	8.0-21	0.68
2	3.01	0.161	5.6-21	0.90
3	1.65	0.825	3.0-21	2.48
4	1.00	0.866	1.0-21	0.87
5	6.72-1	1.41	7.0-22	0.99
6	4.08-1	3.46	6.8-22	2.35
7	2.47-1	3.98	5.8-22	2.31
8	1.50-1	5.76	4.7-22	2.71
9	8.65-2	7.70	3.8-22	2.93
10	5.25-2	8.27	2.8-22	2.32
11	3.18-2	8.30	2.0-22	1.66
12	1.93-2	7.42	1.1-22	0.82
13	1.17-2	7.24	5.0-23	0.36
14	7.10-3	6.40	3.3-23	0.21
15	3.35-3	8.50	2.0-23	0.17
16	1.23-3	9.81	1.8-23	0.18
17	4.54-4	6.46	1.2-24	0.00
18	1.67-4	4.87	1.0-25	0.00
19	6.14-5	2.39	6.0-26	0.00
20	2.26-5	1.44	1.1-25	0.00
21	8.31-6	0.369	4.5-25	0.00
22	2.38-6	0.99	1.0-23	0.00
23	4.14-7	2.52	1.0-22	0.25
24	1.00-10	1.07	6.0-22	0.64

^a

$$\phi_t = P \left(\sum_{g=1}^G \bar{G}_g^P \phi_g \right)^{-1}$$

$$\phi_t = 10\% / 2.28 \times 10^{-22} \text{ (}\%/\text{n/cm}^2\text{)}$$

$$\phi_t = 4.4 \times 10^{22}$$

$$\sum_g^b \bar{G}_g^P \phi_g = 2.28 - 22$$

Table 8.5

CALCULATION OF FLUENCE REQUIRED TO ATTAIN 10% RESIDUAL TOTAL ELONGATION
IN TYPE 304 STAINLESS STEEL AT THE INNERMOST REGION OF THE INNER
RADIAL SHIELD FOR RADIAL SHIELD CONFIGURATIONS 1, 2 AND
5 AND CORE-BLANKET CONFIGURATION C

Broad Group g	Lower Energy of Group g (MeV)	Normalized Group Flux ϕ_g^a [(n/cm ² ·sec) x 10 ²]	Nominal Group Response Function $\bar{G}_g^p$ [%/(n/cm ²)]	Nominal Group Response $\bar{G}_g^p \times \phi_g^b$ [%/(n/cm ²) x 10 ²³]
1	4.97	0.28	8.0-21	2.24
2	3.01	0.678	5.6-21	3.80
3	1.65	2.22	3.0-21	6.66
4	1.00	2.22	1.0-21	2.22
5	6.72-1	2.76	7.0-22	1.93
6	4.08-1	5.66	6.8-22	3.85
7	2.47-1	5.62	5.8-22	3.26
8	1.50-1	6.98	4.7-22	3.28
9	8.65-2	8.30	3.8-22	3.15
10	5.25-2	8.17	2.8-22	2.29
11	3.18-2	7.63	2.0-22	1.53
12	1.93-2	6.53	1.1-22	0.72
13	1.17-2	6.18	5.0-23	0.31
14	7.10-3	5.36	3.3-23	0.18
15	3.35-3	6.85	2.0-23	0.14
16	1.23-3	7.84	1.8-23	0.14
17	4.54-4	5.21	1.2-24	0.01
18	1.67-4	3.98	1.0-25	0.00
19	6.14-5	1.96	6.0-26	0.00
20	2.26-5	0.954	1.1-25	0.00
21	8.31-6	0.26	4.5-25	0.00
22	2.38-6	0.76	1.0-23	0.01
23	4.14-7	1.39	1.0-22	0.14
24	1.00-10	0.77	6.0-22	0.46

a

$$\phi_t = P \left(\sum_{g=1}^G \bar{G}_g^p \phi_g \right)^{-1}$$

$$\phi_t = 10\% / 3.63 \times 10^{-22} \text{ (}\%/\text{n/cm}^2\text{)}$$

$$^b \sum \bar{G}_g^p \phi_g = 3.63 - 22$$

higher and faster flux at the inner radial shield. Based upon the 10% residual total elongation limit, the combined effects of core zoning and the blanket power production reduce the time for the inner shield to reach the fluence limit from 14.60 to 4.25 years. This, however, represents the "worst" case with the end of blanket cycle-six fissile inventory.

Table 8.6 gives an estimation of the times for the inner radial shield to reach the fluence limit based on the average fissile inventories over the equilibrium cycle for two blanket management schemes. In the four-year cycle scheme, the first row blanket element is shuffled to the second row after two years' exposure and then removed after two more years of exposure. For the six-year cycle scheme the shuffle frequency is three years. Note that the inner radial shield lifetime is over 40% longer using the fissile inventory averaged over the equilibrium cycle for the four-year blanket management.

The fluence level and limit at the innermost stainless steel region of the outer radial shield for each shield configuration is given in Table 8.7. Comparing cases 1A and 1C, it is seen that the harder spectrum resulting from the blanket fissile buildup reduces the total fluence limit from 7.6×10^{22} to 6×10^{22} and, at the same time, the total flux doubles from 9.9×10^{12} to 2×10^{13} . Again it is important to note the increased

Table 8.6

ESTIMATED TIMES FOR INNER RADIAL SHIELD TO REACH TYPE 304
STAINLESS STEEL FLUENCE LIMIT FOR 10% RESIDUAL ELONGATION

Blanket Management Scheme	Estimation Based on Blanket Fissile Inventory	Fissile Inventory (kg)		Time, Inner Radial Shield to Fluence Limit (years/80% Load Factor)
		Blanket Row 1	Blanket Row 2	
6 cycle	At end of cycle 6	147	218	4.25
6 cycle	Averaged over equilibrium cycle	74	192	5.25
4 cycle	Averaged over equilibrium cycle	49	129	6.13

Table 8.7

FLUENCE LEVEL^a AND LIMIT AT INNERMOST STAINLESS STEEL REGION OF THE OUTER RADIAL SHIELD FOR EACH RADIAL SHIELD CONFIGURATION
(Based on Fluence Limit to Attain 10% Residual Total Elongation in Type 304 Stainless Steel)

Case		Total Fluence Limit (n/cm ²)	Total Fluence (30 years/ 80% load factor) (n/cm ²)	Effective E > 0.1 MeV Fluence Limit (n/cm ²)	Fast Fluence E > 0.1 MeV (30 years/ 80% load factor) (n/cm ²)	Fluence Margin	Percent of Damage Response Due to E > 1.0 MeV Flux	Percent of Damage Response Due to E > 0.1 MeV Flux	Percent of Damage Response Due to 2.38 eV < E < 0.1 MeV Flux	Percent of Damage Response Due to E < 2.38 eV Flux
Radial Shield Configu- ration	Core- Blanket Combi- nation									
1 & 2	A	7.6 + 22	7.4 + 21	7.4 + 21	7.2 + 20	10	13	49	18	33
1 & 2	C	6.0 + 22	1.5 + 22	9.0 + 21	2.3 + 21	3.9	17	63	17	20
3 & 4	A	7.1 + 22	4.8 + 22	4.8 + 21	3.2 + 21	1.5	17	38	14	48
5	A	4.6 + 22	1.8 + 22	1.4 + 21	5.9 + 20	2.4	8	14	4	82

^aDivide fluence by 7.6×10^8 seconds to obtain flux.

fraction of the damage due to the higher energy flux when the blanket fissions are included. However, based on a 30-year plant life at 80% load factor, the inner radial shield sufficiently reduces flux levels, including a factor of three margin for calculational uncertainty, such that the stainless steel fluence limit for permanent structure will not be exceeded in the outer shield.

Table 8.7 shows that the 30-year fluence levels at the outer radial shield for shield configurations 3, 4 and 5, which have predominantly graphite inner shields, are just below fluence limits even though the effect of the blanket fissions is not included. Almost half of the damage response at the outer radial shield is due to the thermal flux for configurations 3 and 4; and 82% of the damage response is due to the thermal flux for configuration 5, which contains the larger graphite region in the inner radial shield. It appears that an attenuating material should be included in the inner radial shield.

The fluence levels and limits at the PCRV liner for each radial shield configuration are given in Table 8.8. Comparing results for cases 1A and 1C, it is seen that the blanket fissile buildup increases the total flux at the PCRV liner by almost a factor of 4, from 3.2×10^9 to 1.2×10^{10} . The $E > 0.1$ MeV flux, which causes 82% of the damage response, increases by about the same ratio. The spectrum is very similar in both cases so that the total fluence limit remains about the same at 3.8×10^{19} . The fluence limit calculation for case 1A is given in Table 8.9 and is based on the damage function obtained from Ref. 5. Based on a 30-year plant life at 80% load, the radial shield configuration 1 sufficiently reduces flux levels, including a factor of about three error margin, such that the PCRV liner material property change limit will not be exceeded.

Radial shield configurations 2, 3, and 4 also provide adequate flux attenuation to meet the liner fluence limit. Assuming a factor of four increase in the computed fluence due to blanket fissile buildup, each of these configurations still enables the limit to be met with at least a

Table 8.8

FLUENCE LEVEL^a AND LIMIT AT PCRV LINER FOR EACH RADIAL SHIELD CONFIGURATION
(Based on Fluence Limit to Attain a 75°C Shift in the Nil Ductility Temperature)

Case		Total Fluence Limit (n/cm ²)	Total Fluence (30 years/ 80% load factor) (n/cm ²)	Effective E > 0.1 MeV Fluence Limit (n/cm ²)	Fast Fluence E > 0.1 MeV (30 years/ 80% load factor) (n/cm ²)	Fluence Margin	Percent of Damage Response Due to E > 1.0 MeV Flux	Percent of Damage Response Due to E > 0.1 MeV Flux	Percent of Damage Response Due to 2.38 eV < E < 0.1 MeV Flux	Percent of Damage Response Due to E < 2.38 eV Flux
Radial Shield Configu- ration	Core- Blanket Combi- nation									
1	A	3.8 + 19	2.4 + 18	1.2 + 19	7.6 + 17	16	30	82	17	1
1	C	3.7 + 19	9.0 + 18	1.2 + 19	3.0 + 18	4	30	82	17	1
2	A	7.2 + 19	6.8 + 18	1.3 + 19	1.2 + 18	11	3	66	32	2
3	A	5.0 + 19	5.0 + 18	1.2 + 19	1.2 + 18	10	24	77	22	1
4	A	7.4 + 19	3.5 + 18	1.3 + 19	6.2 + 17	21	6	69	29	2
5	A	2.9 + 19	5.9 + 18	1.1 + 19	2.3 + 18	5	40	87	12	1

^aDivide fluence by 7.6×10^8 seconds to obtain flux.

Table 8.9

CALCULATION OF FLUENCE REQUIRED TO ATTAIN A 75°C NIL DUCTILITY
TEMPERATURE SHIFT AT THE PCRV LINER FOR RADIAL SHIELD
CONFIGURATION 1 AND CORE-BLANKET CONFIGURATION A

Broad Group g	Lower Energy of Group g (MeV)	Normalized Group Flux ϕ_g^a [(n/cm ² ·sec) x 10 ²]	Nominal Group Response Function $\bar{G}_g^{pa}$ [°C/(n/cm ²)]	Nominal Group Response $\bar{G}_g^p \times \phi_g^b$ [°C/(n/cm ²) x 10 ¹⁹]
1	4.97	0.55	1.6-17	0.848
2	3.01	0.448	1.4-17	0.627
3	1.65	1.78	1.3-17	2.31
4	1.00	0.257	8.0-18	2.06
5	6.72-1	3.25	6.5-18	2.11
6	4.08-1	5.02	5.0-18	2.51
7	2.47-1	6.22	3.4-18	2.12
8	1.50-1	5.67	3.2-18	1.81
9	8.65-2	5.76	3.0-18	1.73
10	5.25-2	6.83	2.5-18	1.71
11	3.18-2	5.29	1.9-18	1.01
12	1.93-2	6.87	1.0-18	0.687
13	1.17-2	4.21	8.0-20	0.034
14	7.10-3	2.83	7.0-21	0.002
15	3.35-3	6.66	6.5-21	0.004
16	1.23-3	9.60	3.0-21	0.003
17	4.54-4	6.28	1.0-19	0.063
18	1.67-4	4.45	9.0-21	0.004
19	6.14-5	3.01	6.0-21	0.002
20	2.26-5	2.03	9.0-21	0.002
21	8.31-6	1.77	1.4-20	0.002
22	2.38-6	1.97	3.0-20	0.006
23	4.14-7	2.36	8.0-20	0.019
24	1.00-10	4.59	2.6-19	0.119

a

$$\phi t = P \left(\sum_{g=1}^G \bar{G}_g^p \phi_g \right)$$

$$\phi t = 75^\circ\text{C} / 1.972 \times 10^{-18} \text{ (}^\circ\text{C/n/cm}^2\text{)}$$

$$\phi t = 3.8 \times 10^{19} \text{ n/cm}^2$$

b

$$\sum_g \bar{G}_g^p \phi_g = 1.972 - 18$$

factor of two margin for calculational uncertainty. Shielding configuration 5, which is composed primarily of graphite, may marginally meet the liner fluence limit when the effect of blanket fissions is included.

8.5. GAMMA HEATING CALCULATIONS

The gamma heating calculations were performed using 39 groups (24 neutron and 15 gamma), made up of coupled cross section sets that were previously evaluated from the ENDF/B-III data. The calculations did not include the effects of core zoning and of blanket fissile buildup. Due to the unavailability of gamma production data for Th-232 and U-233, the calculations were based on a BOL two-row uranium blanket, combination 1.

Heating rates were evaluated for radial shield configurations 1, 2 and 5 to enable the representative temperature profiles to be computed. The results are shown in Table 8.10 and indicate that the maximum heating rate in the PCRV is below the 2 mW/cm^3 limit for each case. It is expected that configurations 3 and 4 also meet the PCRV heating rate limit.

Table 8.10
MAXIMUM PCRV HEATING RATES

Radial Shield Configuration	Maximum PCRV Heating Rate (mW/cm^3)
1	0.70
2	0.13
5	1.4

8.6 CONCLUSIONS

The following conclusions follow from the results presented above:

1. The effects of core enrichment zoning and blanket fissile buildup must be included to accurately predict fluence levels and heating rates within the GCFR shielding.

2. Based on the design criteria for type 304 stainless steel shielding components, the current inner radial shield cannot meet the criteria for permanent structures and will have to be made replaceable.
3. No additional radial shielding is required to meet the liner fluence limit with the two-row radial blanket.

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IX. REACTOR SYSTEMS ENGINEERING (189a No. SU019)

Under this task, reactor system development activities are being defined and carried out. Analytical methods and models applicable to the assessment of thermal-hydraulic performance of the GCFR reactor core are being developed and utilized to define operating strategies. Methods and materials behavior models are being evaluated to assess the capability of the PCRVR internal structures to serve as a post-accident fuel containment (PAFC); and GCFR plant control systems are being developed, including establishing the interface requirements between these control systems, the plant protection system, the operational protection system, and the plant operator.

9.1. CORE THERMAL-HYDRAULIC PERFORMANCE

Activities in this subtask are devoted to the development of accurate computer models for the evaluation of core thermal-hydraulic performance. In addition, the requirements for and methods of core temperature monitoring are being investigated.

In the previous reporting period, continued development of the core thermal-hydraulics computer program, GACCOOL, was described and preliminary results were reported. During the current reporting period, code development and documentation were continued. Additional orificing cases were studied and the results are reported.

Efforts to define the feasibility of an alternative core temperature monitoring concept were continued.

9.1.1. GACCOOL Development

The computer program, GACCOOL, has been under development as a means to predict the thermal-hydraulic performance of the GCFR core. The flow requirements for each core element are dictated by the power rating in the fuel rod having the highest temperature and by the midwall cladding hot spot temperature limit. The two GACCOOL options, described in previous GCFR quarterly reports, can predict the core thermal-hydraulic performance under (1) ideal and (2) non-ideal orificing conditions. In the idealized case, the individual core element flow rates are determined so as to be consistent with the midwall cladding hot spot temperature limit, and each core element has its own individually sized orifice. The non-ideal core performance option allows the grouping of core elements having similar flow rates into a set of core elements, each of which is fitted with an identical standard size orifice. This grouping results in the overcooling of some core elements and a slight degradation in the mixed-mean core coolant outlet temperature; however, it reduces the number of different orifice types.

During this quarterly period, an option was added to GACCOOL that allows an off-design point evaluation of core performance. In its current state, this option will not handle the extreme low-flow condition where laminar flow prevails and buoyancy effects may become important, but it does allow the calculation of core performance at less than full power. The option searches for and calculates the maximum core element flow resistance. This resistance may not occur in the maximum powered element during off-design conditions because of an altered coolant flow distribution in the core and different orifice flow resistance characteristics in the various core elements. This performance option has been debugged using design point data to reproduce the same results obtained using the normal GACCOOL options.

Additional development activity during this quarter includes the complete revision of the GASFLO subroutine that performs heat transfer calculations and determines the required subchannel flow rate to cool the

fuel rod having the highest temperature in each core element. The revised subroutine is general in that it has a single heat transfer model capable of treating both fuel and blanket elements, and improves upon approximation methods in the previous model. Among the changes was a modification of the heat transfer model for both the fuel and blanket elements to allow the user to either specify the location of the highest midwall cladding temperature or to have the location determined by an iterative positional search. Previously, the location of the highest midwall cladding temperature was assumed to be the lower fuel-axial blanket interface for fuel elements, and the iterative positional search was performed for the blanket elements only. This revision of the subroutine allows the complete temperature distribution along the fuel rod having the highest temperature in each core element to be an output option of GACOOOL. Also, heat transfer multipliers as a function of Reynolds number were added for the roughened portion of the fuel rod. Previously, a constant multiplier had been used. A method was also added to improve the rate of convergence in all of GACOOOL's iterative calculations.

In addition to computer program development activities, the first draft of a document describing GACOOOL was prepared.

9.1.2. Preliminary Core Orificing Results

In the last quarterly report, the effect on core performance of grouping core elements with similar flow requirements into a limited number of standard orifice sizes was presented. During this quarter, similar results were reproduced, which demonstrated a 5°C (9°F) to 7°C (13°F) drop in reactor coolant outlet temperature as the result of grouping ideal orifices into five standard orifice sizes for the fuel elements and four standard orifice sizes for the blanket elements.

During this quarter, orificing studies were performed to determine the effect of reducing the frequency at which orifices are assumed to be reset. For this study, "equilibrium" power distributions for three cycles

(generated from nuclear burnup analyses) and hot spot factors were obtained from Ref. 1. For the case of ideal orificing and where orifices were assumed to be continuously variable, the mixed-mean reactor coolant outlet temperature was calculated to be 564°C (1047°F). For the case of fixed, annually replaceable orifices, which are sized on the basis of the larger of the two coolant flow requirements at the beginning-of-cycle (BOC) or end-of-cycle (EOC), a mixed-mean core outlet temperature of 555°C (1031°F) was obtained. If each orifice were individually sized for each core element as if it were fixed and non-replaceable, in other words on the basis of each core element's largest coolant flow requirement over its entire three-cycle residence time, the resulting mixed-mean core coolant outlet temperature is 542°C (1008°F).

Fixed, non-replaceable orificing is essentially the approach adopted for the Clinch River breeder reactor plant. Burnup in the fuel elements thus contributes to a relatively low peak cladding temperature at the end of their residence time in the core when internal fission gas pressure is the highest. The GCFR utilizes a vented fuel design and may thus depart from this type of orificing strategy. A preliminary estimate was made of the time required to replace an orifice within the reactor cavity using the refueling equipment. Although not insignificant, 20 minutes per orifice, the gain in core performance for a fixed cladding temperature limit is, as indicated above, significant. The replaceable orifice also represents an added degree of freedom to maintain high reactor performance even for unanticipated reactor operating histories.

An investigation was also made to see whether or not a "blank" orifice insert, designed like a diffuser, would be beneficial in the maximally powered element by minimizing the exit pressure drop. Results indicate that if the lower end of the fuel element is carefully designed, the blank orifice insert will probably not be necessary. This is desirable because it reduces the number of orifice inserts that must be handled during refueling operations. Flow tests are needed to verify these results.

9.1.3. Reactor System Development Program Plan

An outline and schedule for the preparation of the Reactor System Development Program Plan (ERDA milestone) has been prepared. In addition, a preliminary list of work items to be included in the plan has been prepared and is being reviewed.

9.1.4. Alternative Methods for Core Temperature Monitoring

The investigative effort was renewed with discussions with personnel at the Santa Barbara Research Center and at UCSD. No response was received from the Infrared Information Analysis Center contact that was made six months ago. The UCSD contact reaffirmed that materials problems will be the major concern, indicating the need for a materials research program in this area.

9.2. POST-ACCIDENT FUEL CONTAINMENT

The objectives of this subtask are (1) to assess the capability of the structures at the bottom of the reactor cavity to contain core debris associated with a postulated core meltdown arising from a series of very low probability failures, and (2) to define the analytical and experimental studies needed to verify thermal processes associated with core debris containment.

In the previous quarterly report, solution methods and input information for the study of upward heat removal were prepared. In addition, a summary of the development program plan was prepared. During the current quarterly period, results of the study of upward heat removal have been obtained, and work on computer program development has been initiated.

9.2.1. Upward Heat Removal

Calculations of the upward heat removal have been partially completed. Results have been obtained by using the present 300-MW(e) GCFR reference

design configuration. Due to the uncertainties in some of the physical properties and the preliminary nature of the reference design configuration, this study is basically parametric. First, a set of most probable input data was prepared by using best-estimate property values and dimensions of the current reference design configuration. Next, input data were varied to determine the sensitivity of analytical results to these changes.

9.2.1.1. Initial Configuration and Assumptions. The computational model based on the reference design configuration is shown in Fig. 9.1. For simplicity, the inner and outer radial shields have been combined into one cylindrical shell. Because the thermal conductivity and volumetric heat capacity values of graphite and stainless steel are similar, the radial shields are assumed to consist of one material having the average property values of graphite and stainless steel. The grid plate was replaced by an equivalent solid disc having the equivalent properties of the perforated plate. The lower boundary of the computational model was fixed at the lower surface of the stainless steel layer of the debris mass. The sides of the stainless steel layer were assumed in contact with the thermal barrier. A uniform heat flux, representing the upward flowing decay heat, was assumed at the lower boundary. Based on the previous one-dimensional heat transfer studies, two-thirds of the heat generated from the decay heat sources in fuel melt was assumed to flow upward. Volatile fission products were assumed to have been released from the fuel into the reactor cavity. Internal free convection within the molten stainless steel layer was treated as an equivalent heat conduction with an assumed effective thermal conductivity. Emissivity of the molten stainless steel surface was assumed to be the same as that of solid stainless steel.

The initial temperatures are given as follows:

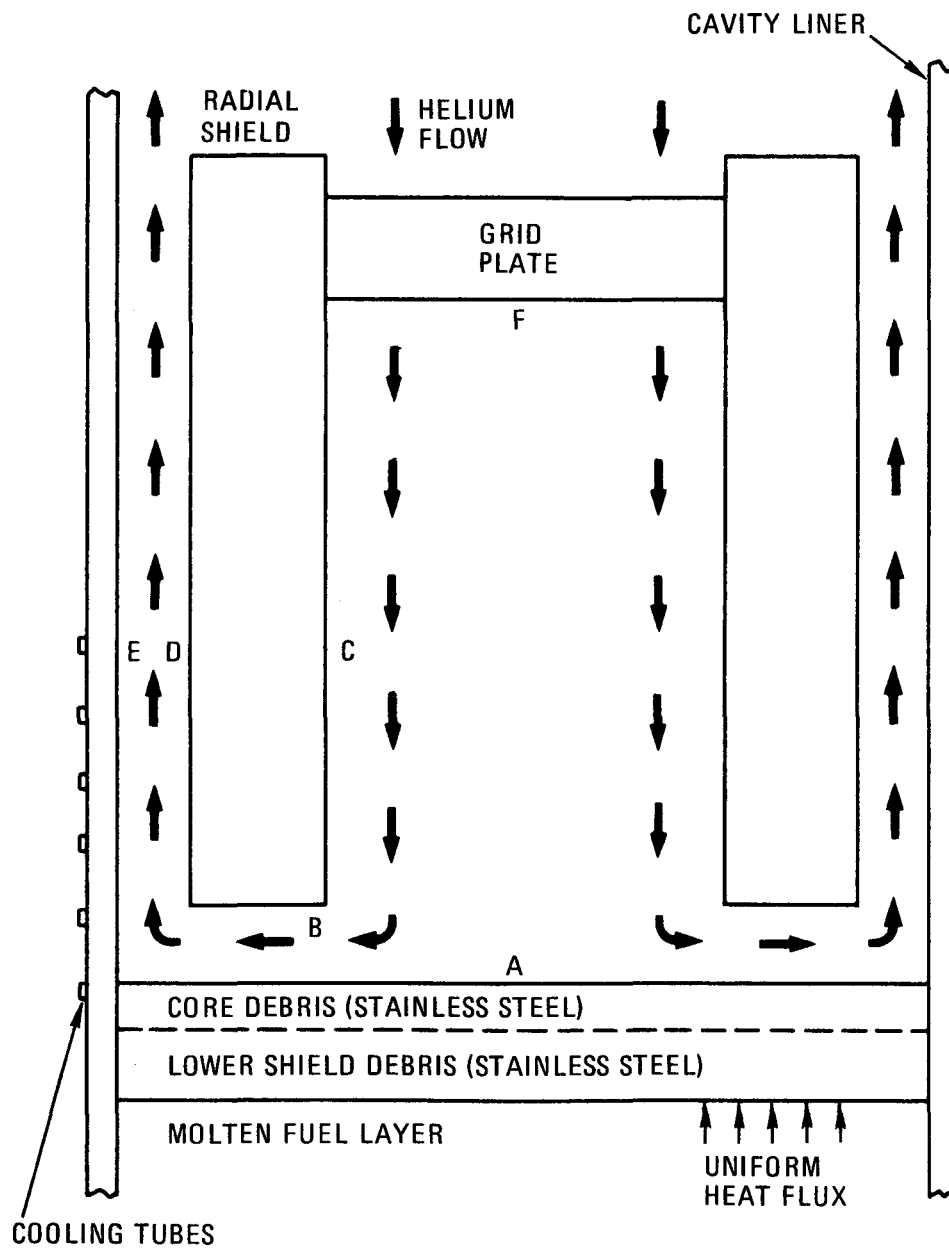


Fig. 9.1 Computational model for upward heat removal to the reactor internal structures

<u>Location</u>	<u>Initial Temperature [°C (°F)]</u>
Radial shields and grid plate	538 (1000)
Thermal barrier	260 (500)
Stainless steel (from core debris)	538 (1000)
Stainless steel (from lower shield)	1427 (2600)
Helium (inlet)	204 (400) - (constant)
Cooling water	38 (100) - (constant)

The heat transfer coefficients for the various surfaces along the helium flow path were calculated by using existing correlations⁽²⁾ and are based on the assumption that only one core auxiliary cooling loop is operating. They are as follows:

<u>Location</u>	<u>Heat Transfer Coefficient (W/m²·°C (Btu/hr·°F·ft²))</u>
Grid plate - upper and lower surface	127 (22)
Radial shield - inner surface	6 (1)
- outer surface	22 (4)
- lower end surface	43 (8)
Thermal barrier - inner surface	22 (4)
Debris mass - upper stainless steel surface	27 (5)

9.2.1.2. Results of the "Most Probable Input Data" Analysis. The following is the best estimate for the most probable input data:

Distance between debris surface and lower end of radial shield	0.3 m (1.0 ft)
Emissivity of molten stainless steel surface	0.6
Combined thermal conductivity of radial shield (graphite and stainless steel)	26 W/m·°C (15 Btu/hr·°F·ft)

Equivalent thermal conductivity of molten stainless steel layer	1730 W/m·°C (1000 Btu/hr·°F·ft)
Fraction of decay heat flowing upward from fuel melt	2/3

Results of the analysis are shown in Figs. 9.2 and 9.3. In Fig. 9.2 the surface temperatures of various locations (shown in Fig. 9.1) are shown as a function of time. From the figure it can be seen that the stainless steel layer in the debris mass is completely molten one hour after the accident but refrozen after about 10 hours. The surface temperature at the lower end of the radial shield remains always below the melting point. As a result of effective cooling by radiation and convection, temperatures at both sides of the radial shield, the grid plate, and thermal barrier are considerably lower than the temperature at the lower end of the radial shield. Within 15 hours, all surface temperatures have passed their maximum point and approach a quasi-steady state.

In Fig. 9.3 the interior temperature history at various radial shield elevations is shown. As expected, at locations closer to the lower end of the radial shield, the maximum temperature is reached earlier. A heat pulse is traveling upward through the radial shield. A line connecting the maxima at the various elevations shows that the maxima decrease at elevations further away from the heat source, i.e., heat is being stored in the radial shield as the heat pulse travels upward.

9.2.1.3. Parametric Studies. Results from a parametric study are shown in Table 9.1. The temperatures at two critical locations are shown for each parametric study case: A, molten stainless steel surface, and B, lower end of radial shield. The maximum temperature and time of maximum temperature are shown for both locations. In each case only one parameter was varied with the remaining ones kept at their original values.

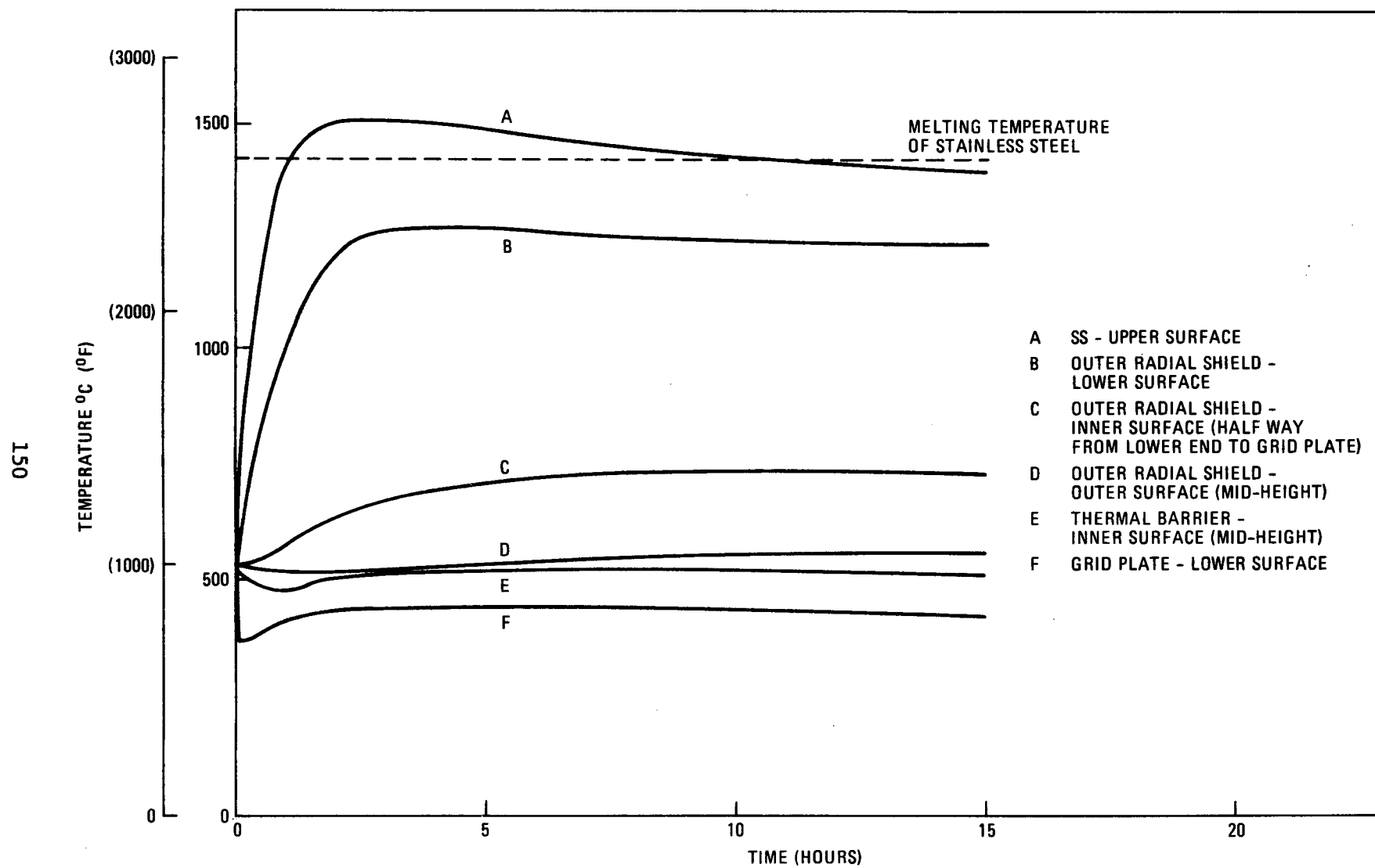


Fig. 9.2 Surface temperature history at various locations within reactor cavity

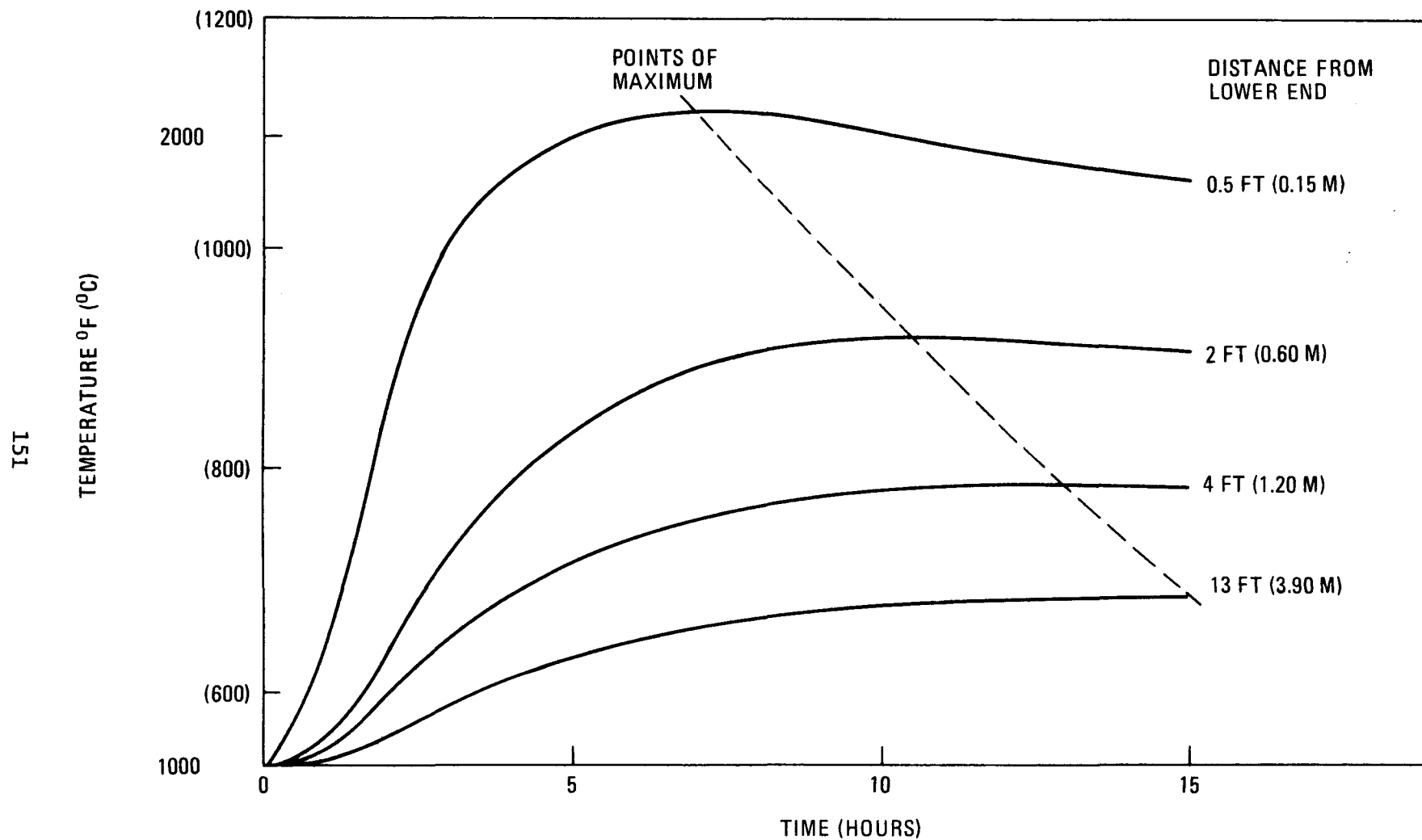


Fig. 9.3 Interior temperature history at various outer radial shield elevations [7 in. (17.8 cm) from the inner surface]

Table 9.1
RESULTS OF PARAMETRIC STUDY ON UPWARD HEAT REMOVAL

Case	Molten Stainless Steel Surface, A		Lower End of Radial Shield, B	
	Temperature (°C [°F])	Time (hr)	Temperature (°C [°F])	Time (hr)
1 - Most probable input data	1510 (2750)	2.4	1279 (2335)	4.4
2 - Emissivity of molten stainless steel surface = 0.9	1496 (2724)	2.0	1302 (2375)	3.5
3 - Thermal conductivity of molten stainless steel layer - 865 W/m·°C (500 Btu/hr·°F·ft)	1507 (2744)	2.6	1276 (2329)	4.5
4 - Combined thermal conductivity of radial shield (graphite and stainless steel) - 21 W/m·°C (12 Btu/hr·°F·ft)	1513 (2755)	2.6	1293 (2360)	4.3
5 - Fraction of decay heat flowing upward from melt - 3/4	1563 (2846)	3.3	1348 (2459)	4.5
6 - Distance between debris surface and lower end of the radial shield - 0.6 m (2 ft)	1575 (2867)	4.4	1423 (2593)	5.0
7 - Heat transfer coefficient at lower end of radial shield - 22 W/m ² ·°C (4 Btu/hr·°F·ft ²)	1589 (2893)	4.7	1456 (2653)	5.0

Case 1. Standard case with the most probable input data.

Case 2. The emissivity of the molten stainless steel surface is increased by 50%. The results show a lower temperature at A and higher temperature at B. This indicates that for this case, the radiative heat transfer is more effective.

Case 3. The equivalent heat transfer coefficient in the molten stainless steel layer is decreased by 50%. As a consequence, the temperatures at both A and B are slightly lower due to the increased heat storage in the molten stainless steel layer.

Case 4. The combined thermal conductivity of the radial shield (graphite and stainless steel) is decreased by 20%. The higher temperatures at A and B are due to less heat being conducted through the radial shield.

Case 5. The fraction of decay heat flowing upward is increased from $2/3$ to $3/4$. As a result, the temperatures at both A and B are higher but still tolerable.

Case 6. The distance between the debris surface and the lower end of the radial shield is doubled. The temperatures at A and B are increased because the helium mass flow velocity through this passage is reduced, thus resulting in a lower heat transfer coefficient.

Case 7. The heat transfer coefficient at the lower end of the radial shield is reduced by 50%. Slight surface melting occurs at B.

For the last two cases, 6 and 7, the temperature at B continues to increase after 5 hours. However, the temperature increase should not exceed $11-17^{\circ}\text{C}$ ($20-30^{\circ}\text{F}$) because the temperatures at A are already approaching maximum value.

9.2.1.4. Conclusion. With the most probable input data, the calculated results have indicated that the temperatures at various locations within the reactor internal structures are not excessively high and that upward flowing decay heat can be effectively removed with only one loop of the core auxiliary cooling system in operation. The parametric study has shown that these results are insensitive to most of the input parameters, the only exception being the heat transfer coefficient at the lower end of the radial shield. However, the calculation of this heat transfer coefficient was based on the assumption of laminar flow passing between two parallel plates. This is believed to be conservative since the entrance flow may already be turbulent but not fully developed.

9.2.2. Computer Program Development

A computer program especially well suited for the post-accident fuel containment analysis is being developed. Existing computer programs are being surveyed, and advanced numerical methods are being developed.

9.3. CONTROL SYSTEMS

A detailed work plan for FY-76 was generated and submitted for review. As indicated in this work plan, the major effort during FY-76 will go into the development of a linear dynamic plant model that is suitable for on-line plant control system analyses.

The mathematical relationships for a dynamic model of the steam generator have been derived. Data have been prepared for testing the responses of this model against those of an existing more detailed non-linear model. The basic assumptions made in conjunction with the development of this model are

1. A steam generator can be represented by a single average tube whose properties represent those of the entire tube bundle.
2. A lumped parameter model is valid.
3. The fluid flow and heat transfer characteristics are adequately represented by a three-node model in which the 0% quality boundary and the 100% quality boundary are free to move. This means that the first node contains only subcooled water, the second node is the two-phase region, and the third node contains dry steam.

REFERENCES

1. "300-MW(e) Gas-Cooled Fast Breeder Reactor Demonstration Plant," General Atomic Report GA-A13045, July 15, 1974.
2. Jakob, M., Heat Transfer, v. 1, John Wiley and Sons, Inc., New York, 1949, Ch. 22 and 26.

X. COMPONENT DEVELOPMENT (189a No. SU025)

10.1. REACTOR VESSEL

The scope of this subtask is: (1) to assure that the design of the PCRVR and the related components that contribute to the integrity of the pressure boundary is satisfactory, and (2) to test critical component configurations to make certain that they attain the design objectives.

This subtask will demonstrate by analyses and tests that the PCRVR with its penetrations and closures will meet the design criteria. It will provide assurance that the design of the thermal barrier will satisfactorily protect the liner and PCRVR from the effects of high temperatures, and that flow restrictors for the large penetrations can be developed to limit the flow of helium from the primary system to acceptable levels, in the event of structural failure of a penetration or closure component.

A draft of the topical report for the PCRVR closure flow restrictor evaluation was prepared for internal review. The report presented: (1) the results of technical evaluation of the reference design and eight alternatives and (2) rated the concepts on a system based on simplicity of manufacture and installation and ease of in-service inspection.

In conjunction with the testing effort by ORNL of the model of the system generator cavity closure, ORNL presented for review to the GCFR Vessel and Internals group detailed drawings of the closure test model and a layout of the testing fixture including the test plan. After review by GA, the drawings and plan were approved. An analytical model was completed for the steam generator cavity closure, and a finite element stress analysis was initiated.

During this quarterly reporting period the work plan for the reactor vessel was issued for final release. The work plan describes the objectives and work programs for the tasks for the PCRV and the components of the thermal barrier, cavity closures, and closure flow restrictors.

The design criteria document, A-DC-11-1, defining the functional and performance requirements and the design and safety requirements for the 300-MW(e) PCRV, has been drafted. When published this document will provide the basis for preparing a test plan and designing appropriately sized scale models of the flow restrictors selected for testing.

In support of the model testing program for the PCRV cavity closures, layouts were prepared for the reinforcing bars (rebars) to be used in the prototype steam generator closure. The geometry, sizes, and placement of the rebars in the HTGR steam generator closure were reviewed for applicability to the GCFR. Based on the similarity of design of the closures and evaluation of the results from the testing of a scale model of the HTGR design, a layout drawing will be prepared for the locations and placement of the rebars in the GCFR prototype closure. The sizes of the rebars to be used, and an estimate of the quantities of linear footage, were transmitted by letter to ORNL.

At a meeting at ORNL on October 1, 1975, the preliminary drawing of the rebar was reviewed. It was proposed by ORNL that the basic pattern of the rebar placement be changed from 10° to 9° to provide greater access for strain gage placement in the model, and that the anchor studs in the prototype closure not be modeled. These conditions were accepted by the GCFR Vessels and Internals group, and confirmed by a telephone communication. The drawing of the prototype closure is being revised to incorporate these revisions.

10.2. CONTROL AND LOCKING MECHANISMS

The primary objective of this task is the preparation of a comprehensive development plan for the control and locking mechanisms of the GCFR demonstration plant.

The work was initiated as planned during the quarter. However, no reportable results had been generated by the end of the reporting period.

10.3. FUEL HANDLING DEVELOPMENT

There are three subtasks covered under fuel handling development:

1. Conceptual studies and system optimization,
2. Spent-fuel shipping studies, and
3. Post-irradiation examination (PIE) facility evaluations.

During this quarterly reporting period, in adherence to the subtask work plan and refueling system development plan schedules, work in these areas was undertaken as described below.

10.3.1. Conceptual Studies and System Optimization

Due to the large number of interfaces between the components, certain components and subsystems of the reference design refueling system were studied and evaluated on an integrated basis. These efforts concentrated on achieving system optimization by improving efficiency in the reference design. Specific areas evaluated during this quarter include:

1. Auxiliary turntable and ram system,
2. Service facility turntable and ram system,
3. Penetration shield plug storage concepts,
4. In-vessel fuel handling machine storage in auxiliary casks,
5. Core element PES vent sealing requirements,
6. Alternative schemes for transfer of spent-fuel from the reactor to the spent-fuel pool.

Flow charts indicating various possible combinations of equipment items aided in analyzing the effects of modifying or eliminating one of

the components. In addition, the handling times involved in utilizing these components were tabulated to obtain a relative indication of overall efficiency of the modified system.

Preliminary results to date indicate that if certain modifications were made to the reference design, refueling preparation operation time could be reduced more than 20%. These recommendations are

1. Provision of three PCRV refueling penetrations, instead of four, all identical in size (eliminate the 90° offset PCRV penetration);
2. Elimination of both auxiliary and service facility turntables (switch track in reactor service building to replace service facility turntable);
3. Provision of storage penetrations in the PCRV bottom face for the refueling penetration shield plugs and storage of the four in-vessel fuel handling machines in penetrations within the reactor service facility which results in the reduction of the auxiliary transfer casks from six, two large, four small, to one large cask;
4. Replacement of each dual ram system of the eliminated turntables with a single, movable ram;
5. Incorporation of a shield plug lifting cask to transfer the refueling penetration shield plugs.

In addition to the increased efficiency, the potential savings on capital equipment is quite substantial. These preliminary results are being utilized in the refueling system optimization effort of the scheduled subtask.

10.3.2. Spent-Fuel Shipping Studies

During this quarter, the only scheduled objective was information gathering. A literature search resulted in obtaining the complete proceedings of the second, third and fourth international symposia (1968, 1971, and 1974) on the packaging and transportation of radioactive materials. The search indicated that of the few hundred papers discussed, approximately 45 are relevant to our subtask topic. These in turn contain further references, which may also be of use. In addition, vendors in this field were contacted for information on available equipment and services.

10.3.3. Post-Irradiation Examination (PIE) Facility Evaluations

As with spent-fuel shipping studies, data gathering was the sole item scheduled under this subtask. Information on a similar facility at Commonwealth Edison's Dresden-1 plant was obtained at the ANS Reactor Operating Experience Conference in Albuquerque. In addition, vendors of remote fuel examination equipment were contacted, and drawings and data were obtained on related equipment.

10.4. CORE SUPPORT STRUCTURE

The purpose of this subtask is to assure the development of structural analysis methods and information on materials mechanical behavior required to assess the structural integrity of the GCFR core support structure under all anticipated operational and safety-related loading conditions in the GCFR environment.

In the previous quarterly period,⁽¹⁾ the thickness effect on the grid plate deflection was investigated by means of isotropic elasticity theory. In addition, an analytical solution was developed for predicting the seismic response of the grid plate and of the coupled system including the core elements.

During this quarterly period, the work was further developed in the area of grid plate structural behavior. An analytical solution, which includes the anisotropic effect resulting from the perforation of the plate, was obtained from the anisotropic elasticity theory. Moreover, a 15% scale model of the grid plate and the core elements was constructed to determine the static characteristics and the dynamic response of the grid plate.

10.4.1. Structural Analysis

When applying the concept of the equivalent solid plate to the grid plate analysis, it should be recognized that, in general, the equivalent solid material of the plate does not behave isotropically. In fact, for a triangular pattern of perforations, the elastic properties in the plate in-plane directions could be quite different from those in the direction perpendicular to the plane of the plate. As was noted by Slot⁽²⁾ the effect of anisotropy on the deflections of thick plates could be pronounced. In view of this and the effect of the grid plate deflection on the reactivity response of the core, it is important to carry out an investigation of the anisotropic effect on the grid plate structural behavior.

The problem was formulated in such a way that the grid plate was simply supported at the edge and was uniformly loaded by distributed forces, q , as was shown in Fig. 10.1. In addition, the plate was assumed to be made of a transversally isotropic material having the axis of symmetry coincident with the z coordinate axis (see Fig. 10.1). Because the problem is axisymmetric, the stress-strain relationships, using the conventional notation for the cylindrical coordinates, reduce to:

$$\begin{aligned}
 \epsilon_r &= \frac{1}{E} \sigma_r - \frac{\nu}{E} \sigma_\theta - \frac{\nu'}{E'} \sigma_z , \\
 \epsilon_\theta &= -\frac{\nu}{E} \sigma_r + \frac{1}{E} \sigma_\theta - \frac{\nu'}{E'} \sigma_z , \\
 \epsilon_z &= -\frac{\nu'}{E'} (\sigma_r + \sigma_\theta) + \frac{1}{E'} \sigma_z , \\
 \gamma_{rz} &= \frac{1}{G'} \tau_{rz} ,
 \end{aligned} \tag{1}$$

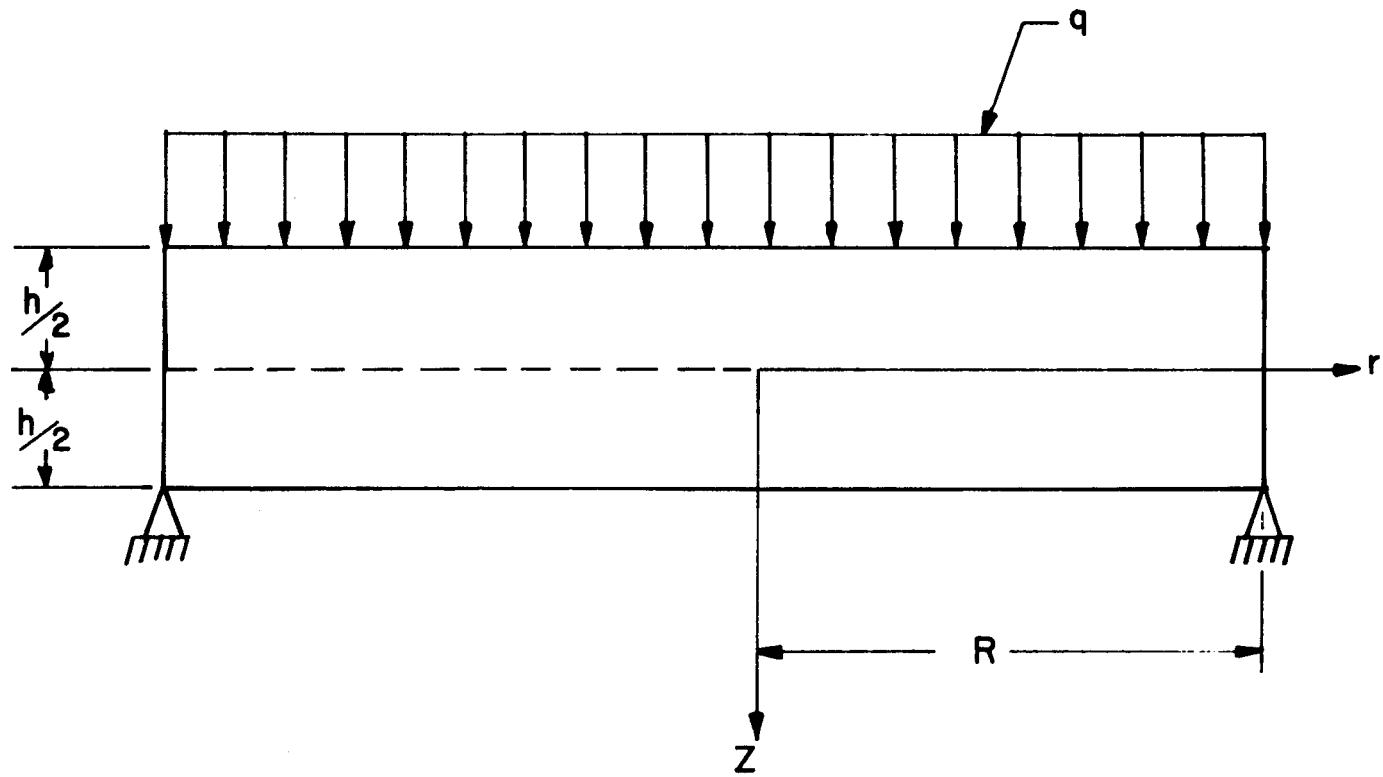


Fig. 10.1 Simply-supported plate

where E , E' are the Young's moduli with respect to the plane of isotropy and perpendicular to it; ν is the Poisson's ratio that characterizes the transverse reduction in the plane of isotropy for tension in the same plane; ν' is the Poisson's ratio that characterizes the transverse reduction in the plane of isotropy for the tension in a direction normal to it; G' is the shear modulus for the plane normal to the plane of isotropy. Moreover, the axisymmetric deformation of the plate can be specified by the displacement functions:

$$\begin{aligned} U &= U_r(r, z) , \\ V &= 0 , \\ W &= W(r, z) , \end{aligned} \tag{2}$$

where U , V , W are displacements in radial, tangential and axial directions, respectively.

The equations of equilibrium are of the form:

$$\begin{aligned} \frac{\partial \sigma_r}{\partial r} + \frac{\partial \tau_{rz}}{\partial z} + \frac{\sigma_r - \sigma_\theta}{r} &= 0 , \\ \frac{\partial \tau_{rz}}{\partial r} + \frac{\partial \sigma_z}{\partial z} + \frac{\tau_{rz}}{r} &= 0 . \end{aligned} \tag{3}$$

and the compatibility conditions can be expressed by:

$$\begin{aligned} \epsilon_r - \frac{\partial}{\partial r} (r \epsilon_\theta) &= 0 , \\ \frac{\partial^2 \epsilon_r}{\partial z^2} + \frac{\partial^2 \epsilon_z}{\partial r^2} - \frac{\partial^2 \gamma_{rz}}{\partial r \partial z} &= 0 . \end{aligned} \tag{4}$$

Following Lekhnitskii,⁽³⁾ the problem can be solved by the method using the stress function, ϕ , defined by:

$$\begin{aligned}
 \sigma_r &= -\frac{\partial}{\partial z} \left(\frac{\partial^2 \phi}{\partial r^2} + \frac{b}{r} \frac{\partial \phi}{\partial r} + a \frac{\partial^2 \phi}{\partial z^2} \right), \\
 \sigma_\theta &= -\frac{\partial}{\partial z} \left(b \frac{\partial^2 \phi}{\partial r^2} + \frac{1}{r} \frac{\partial \phi}{\partial r} + a \frac{\partial^2 \phi}{\partial z^2} \right), \\
 \sigma_z &= \frac{\partial}{\partial z} \left(c \frac{\partial^2 \phi}{\partial r^2} + \frac{c}{r} \frac{\partial \phi}{\partial r} + d \frac{\partial^2 \phi}{\partial z^2} \right), \\
 \tau_{rz} &= \frac{\partial}{\partial r} \left(\frac{\partial^2 \phi}{\partial r^2} + \frac{1}{r} \frac{\partial \phi}{\partial r} + a \frac{\partial^2 \phi}{\partial z^2} \right),
 \end{aligned} \tag{5}$$

where:

$$\begin{aligned}
 a &= \frac{-\frac{\nu'}{E'} \left(\frac{1}{E} + \frac{\nu}{E} \right)}{\frac{1}{EE'} - \left(\frac{\nu'}{E'} \right)^2}, \\
 b &= \frac{-\frac{\nu'}{E'} \left(\frac{1}{G} - \frac{\nu'}{E'} \right) + \frac{\nu}{EE'}}{\frac{1}{EE'} - \left(\frac{\nu'}{E'} \right)^2}, \\
 c &= \frac{-\frac{\nu'}{E'} \left(\frac{1}{E} + \frac{\nu}{E} \right) + \frac{1}{EG'}}{\frac{1}{EE'} - \left(\frac{\nu'}{E'} \right)^2}, \\
 d &= \frac{\left(\frac{1}{E} \right)^2 - \left(\frac{\nu}{E} \right)^2}{\frac{1}{EE'} - \left(\frac{\nu'}{E'} \right)^2}.
 \end{aligned} \tag{6}$$

Equations 5 satisfy the first equation of Eq. 3 and all of Eq. 4. As a result the stress function is governed by the second equation of Eq. 3, which becomes:

$$\begin{aligned}
& \left(\frac{\partial^2}{\partial r^2} + \frac{1}{r} \frac{\partial}{\partial r} \right) \left(\frac{\partial^2 \phi}{\partial r^2} + \frac{1}{r} \frac{\partial \phi}{\partial r} + a \frac{\partial^2 \phi}{\partial z^2} \right) \\
& + \frac{\partial^2}{\partial z^2} \left(c \frac{\partial^2 \phi}{\partial r^2} + \frac{c}{r} \frac{\partial \phi}{\partial r} + d \frac{\partial^2 \phi}{\partial z^2} \right) = 0 \quad .
\end{aligned} \tag{7}$$

For this particular problem, the solution of the stress function in Eq. 7 can be expressed in the form of homogeneous polynomials of the third, fourth, and sixth degrees:

$$\begin{aligned}
\phi = & A_1 Z^3 + A_2 Z r^2 + A_3 (Z^4 - \frac{3}{8} d r^4) \\
& + A_4 (Z^2 r^2 - \frac{a+c}{8} r^4) + A_5 \left[Z^6 - \frac{45}{8} d Z^2 r^4 \right. \\
& + \frac{5}{16} (a+c) d r^6 \left. \right] + A_6 \left[Z^4 r^2 - \frac{3}{4} (a+c) Z^2 r^4 \right. \\
& + \left. \frac{(a+c)^2 - d}{24} r^6 \right] \quad .
\end{aligned} \tag{8}$$

The arbitrary constants $A_1, A_2, \dots, A_6$ in Eq. 8 are determined from the boundary conditions which are

$$\sigma_z = \tau_{rz} = 0, \quad Z = \frac{h}{2} \quad ,$$

$$\sigma_z = -q, \quad \tau_{rz} = 0, \quad Z = -\frac{h}{2} \quad ,$$

$$\int_{-\frac{h}{2}}^{\frac{h}{2}} \sigma_r dz = 0, \quad r = R \quad , \tag{9}$$

$$\int_{-\frac{h}{2}}^{\frac{h}{2}} \sigma_r z dz = 0 \quad r = R \quad .$$

The first two equations of Eq. 9 specify exactly the conditions on the plate surfaces, while the last two equations describe the approximate conditions of the lateral surface in such a way that the radial resultant force and bending moment are zero. By Saint Venant's Principle, the solution is valid away from the edge of the plate.

With the arbitrary constants in the stress function being determined by the boundary conditions, the stresses are readily obtained. They are

$$\begin{aligned}
 \sigma_r &= \frac{3q}{4h^3} (3 + \nu) (R^2 - r^2)Z + qm \left[\left(\frac{z}{h} \right)^3 - \frac{3}{20} \frac{z}{h} \right] , \\
 \sigma_\theta &= \frac{3q}{4h^3} [(3 + \nu)R^2 - (1 + 3\nu) r^2]Z + qm \left[\left(\frac{z}{h} \right)^3 - \frac{3}{20} \frac{z}{h} \right] , \\
 \sigma_z &= \frac{q}{2} \left[-1 + 3 \frac{z}{h} - 4 \left(\frac{z}{h} \right)^3 \right] , \\
 \tau_{rz} &= -\frac{3q}{4h} r \left[1 - 4 \left(\frac{z}{h} \right)^2 \right] ,
 \end{aligned} \tag{10}$$

where

$$m = \frac{E}{1-\nu} \left[\frac{1}{G'} - \frac{\nu'(3+\nu)}{E'} \right] .$$

Substituting Eq. 10 into the stress-strain relationships, Eq. 1, and bearing in mind that

$$\begin{aligned}
 \epsilon_r &= \frac{\partial u}{\partial r} , \quad \epsilon_\theta = \frac{u}{r} , \\
 \epsilon_z &= \frac{\partial w}{\partial z} , \quad \gamma_{rz} = \frac{\partial u}{\partial z} + \frac{\partial w}{\partial r} ,
 \end{aligned}$$

the displacements can be obtained by integration:

$$\begin{aligned}
u = & \frac{qr}{E} \left\{ \frac{Ev'}{2E'} + \frac{3(1-\nu)}{4h^3} [(3+\nu)R^2 - (1+\nu)r^2] z \right. \\
& \left. - \frac{3z}{20h} \left[m(1-\nu) + 10 \frac{Ev'}{E'} \right] + \frac{z^3}{h^3} \left[m(1-\nu) - \frac{2Ev'}{E'} \right] \right\} , \\
w = & \frac{q}{64D} (R^2 - r^2) \left[\frac{5+\nu}{1+\nu} R^2 - r^2 \right] + \frac{3q}{10h} \left[\frac{1}{G} - \frac{\nu'(7-\nu)}{4E'} \right] (R^2 - r^2) \quad (11) \\
& + \frac{qz}{E'} \left\{ -\frac{1}{2} - \frac{3\nu'z}{4E'h^3} [(3+\nu)R^2 - 2(1+\nu)r^2] + \frac{3z}{20h} (m\nu'+5) \right. \\
& \left. - \frac{z^3}{2h^3} (m\nu'+1) \right\} ,
\end{aligned}$$

where

$$D = \frac{Eh^3}{12(1-\nu^2)}$$

It can be shown that when the corresponding elastic constants of the different directions are equal, Eqs. 10 and 11 reduce to Love's isotropic elasticity solution.⁽⁴⁾ Furthermore, the thin plate solution can also be recovered if the plate thickness-to-radius ratio is small.

Based on the analytical results, a computer program, GRIPLAT, was written for calculating stresses and displacements in the grid plate. As a demonstration of the code, a run was made for the grid plate of the reference designs subjected to pressure of 42 psi (290 kPa). The results indicate that the deflections at the center of the middle plane are 0.025 in. (0.64 mm), 0.021 in. (0.53 mm) and 0.019 in. (0.48 mm) for the isotropic elasticity theory, the transversely isotropic elasticity theory, and the thin plate theory, respectively. This is reasonable in that the elasticity theories include the transverse shear effect, which results in additional deflections, and the transversely isotropic material behavior

takes into account the higher elastic modulus in the direction normal to the plane of the plate. In fact, the assumptions made in the customary thin plate theory imply that the plate material is rigid in the direction normal to the plane of the plate.

10.4.2. Model Test of the Core Support Structure

A draft of the topical report for the seismic analysis of the GCFR core support structure was completed in the last quarter. In conjunction with this effort, a 15% scale model of the grid plate and the core was constructed to determine the static characteristics and dynamic response of the grid plate, including the effect of the core elements.

The model of the grid plate is constructed of 7075-T651 aluminum with a diameter of 20 in. (508 mm) and a thickness of 3.6 in. (91 mm). The fuel and blanket elements are constructed from commercially available 6061 aluminum tubes. The outer diameter of the tubes is 0.875 in. (22.22 mm) with a thickness of 0.095 in. (2.41 mm). To satisfy the frequency and mass ratios of these components, steel inserts are placed in the tubes. These models were manufactured by the Sorrento Valley Facility of GA.

Since the design basis of the core support is to minimize reactivity effects associated with these phenomena, and because these newer results tend to increase deflections and concomitant reactivity effects, a test is needed to verify the adequacy of the more sophisticated analysis methods.

Based on similarity laws a test plan was prepared. The test plan includes a static test to determine the effective elastic modulus, the anisotropic constants, and the deflections of the plate as a function of loading. Since the loads on the grid plate of the reactor consist of gas pressure and the weight of the core elements, the test model with an equivalent uniform pressure will simulate these conditions. To determine the required dynamic parameters of the core support system, based

on similarity laws, the test plan is separated into the free vibration test and the simulated seismic excitation.

The free vibration test determines the natural frequencies and the associated mode shape for the system of grid plate, core elements, and the corresponding damping factors. By replacing the steel insert with steel balls in some of the core elements, the effects of the different mass and friction factors on the natural frequencies and damping factors can be investigated.

The simulation of seismic excitation is performed by applying random forcing functions, which approximately simulate a standard seismic excitation. The direction of the excitation is horizontal, or by the application of a special fixture, simultaneous excitation can be applied horizontally and vertically. During excitation, the important lower frequencies and mode shapes will be determined with the corresponding relative amplitudes (between adjacent rods) and stresses. With an increased level of excitation, the nature of the contact between rods can also be investigated. The deflection of the plate will be measured by dial indicators and the stresses by strain gauges.

The frequencies and the corresponding mode shapes of the structure can be determined by the combination of accelerometers, high speed cameras, and a strobe light. Additional strain gauges will be helpful to determine the dynamic stresses and amplitudes during contact. A request for proposals outlining the test to be performed was sent to several companies, and four of them responded positively.

In addition, during this quarter, a paper entitled "Seismic Model of the GCFR Core Support Structure" by L. E. Penzes was published at the ELCALOP Symposium in Berlin, Germany (paper #U5/6, September 8-11, 1975).

10.5. REACTOR SHIELDING ASSEMBLIES

Responsibility for the development of analytical and experimental

methods to insure a reliable structure and to insure an efficient thermal-hydraulic design of the shields falls under this subtask. In the previous quarterly report⁽¹⁾ the work plan of the shield was discussed in some detail. In this quarter the work plan, including thermal-hydraulic studies and structural analyses of these components, was completed and issued.

A preliminary stress and deflection analysis of the outer and inner radial shield did not indicate any problems either for the outer or the inner radial shields.

A review of the CRASIB computer code was completed to determine its applicability to the structural analysis of the radial shields when the effects of high temperature and irradiation are included. The review showed that the code would have to be modified to be directly applicable to the problem. This is not being undertaken this year.

10.5.1. Shielding Structural Analysis

A structural analysis of the inner and outer shields was performed applying conservative, simplifying physical assumptions. The effects of gas pressure, weight of the shields, temperature, creep and irradiation, and induced swelling were considered in the analysis; and types 304 and 316 stainless steel materials were considered for the cladding of the shields in this study. The results of the analysis will be presented separately for each component of the shielding.

When the deformations of the structural components are restricted, the major sources of the shield stresses are due to high temperature and irradiation induced swelling. Because of stress relaxation, both of these stresses will be redistributed and reduced.⁽⁵⁾ For the outer shield of the reference design the preliminary analysis indicates generally acceptable low stresses. The stress values of the shell type of outer radial shield are even lower because of the more flexible nature of the shell structure. The loss of ductility is also insignificant for either of these configurations. The calculated stress, as in the inner radial shield,

is also generally acceptable.⁽⁶⁾ A paper entitled "Generalized Hydrodynamic Effects of a Double Annulus on a Vibrating Cylindrical Shell" by L. E. Penzes and S. K. Bhat was published at the 3rd SMIRT conference in London, England (paper #F216, September 1-5, 1975).

The CRASIB computer code was reviewed for its suitability as a tool for calculating stresses and deformations in the radial shields due to thermal, creep, and irradiation-induced swelling effects. CRASIB is a finite element computer program that evaluates stress and displacements as a function of time according to the simple beam theory. It is believed that the CRASIB mechanical model is adequate as a first approximation for the evaluation of the stresses and deflections of the radial shields. However, in the current version, CRASIB only allows input of a constant temperature gradient through the thickness of the beam section. According to the current calculational results, the temperature profile in the radial shield is highly nonlinear, therefore CRASIB should be modified before it is applied to the shielding structural analysis.

10.6. MAIN HELIUM CIRCULATOR, VALVE AND SERVICE SYSTEM

There was no scheduled activity on this task for this quarterly period.

10.7. STEAM GENERATOR

The purpose of this task is to develop a steam generator that meets the operational, performance, and safety requirements of the GCFR. During this fiscal year several steam generator designs will be analyzed and evaluated, and the merits of each design will be compared.

As part of the evaluation of the reference steam generator configuration and alternatives, a unit using straight tubes, instead of helical tubes, is being investigated. One important difference between the helical tubes with helium cross flow and the straight tubes with parallel helium flow is that the former arrangement permits reasonably high water velocity whereas the latter arrangement, using the closest practical tube spacing, results in a low water velocity.

To determine the effect of reduced mass velocity on critical heat flux, the extensive data of Herkenrath, et al.,⁽⁷⁾ were evaluated for GCFR conditions. Figure 10.2 shows critical steam quality as a function of critical heat flux and mass velocity at a pressure of 2972 psig (20.5 MPa). It may be noted that critical quality increases with mass velocity at a given heat flux, whereas at a given mass velocity, critical heat flux decreases as quality increases. The curves shown for mass velocities less than 0.52×10^6 lb/hr·ft² (700 kg/m²s) are extrapolations of the data. Because of the great difference in mass velocity between the coiled tube and straight tube units, the critical quality will vary from about 80% (coiled) to about 30% (straight). The consequence of this apparent disadvantage for the straight tube is small, because the favorable properties of steam at 2950 psia (20.3 MPa), i.e., very high Prandtl No., result in a relatively large film boiling heat transfer coefficient compared with the much smaller helium heat transfer coefficient. Thus, the overall heat transfer coefficient in the evaporator decreases moderately when nucleate boiling ceases and film boiling prevails. If desired, the nucleate boiling region can be extended to extremely high steam qualities by the use of helical ribs on the inside tube surface, as demonstrated by Swenson, et al.⁽⁸⁾

Using the 300-MW(e) design conditions, an initial thermal-hydraulic analysis has been made for a straight tube steam generator bundle in which helium flows parallel to the outside tube surfaces, and water flows upward inside the tubes in the counter flow direction. A comparison of this representative case with the present reference design (helical coil), for the main bundle, is shown below:

	<u>Straight</u>	<u>Helical</u>
Number of tubes	6396	218
Bundle diameter, ft (m)	7 (2.1)	10 (3.0)
Bundle length, ft (m)	59 (18.0)	24 (7.3)
Tube length, ft (m)	59 (18.0)	716 (218.3)
Helium ΔP , psi (MPa)	9 (0.062)	10 (0.069)
Steam ΔP , psi (MPa)	13 (0.089)	250 (1.72)

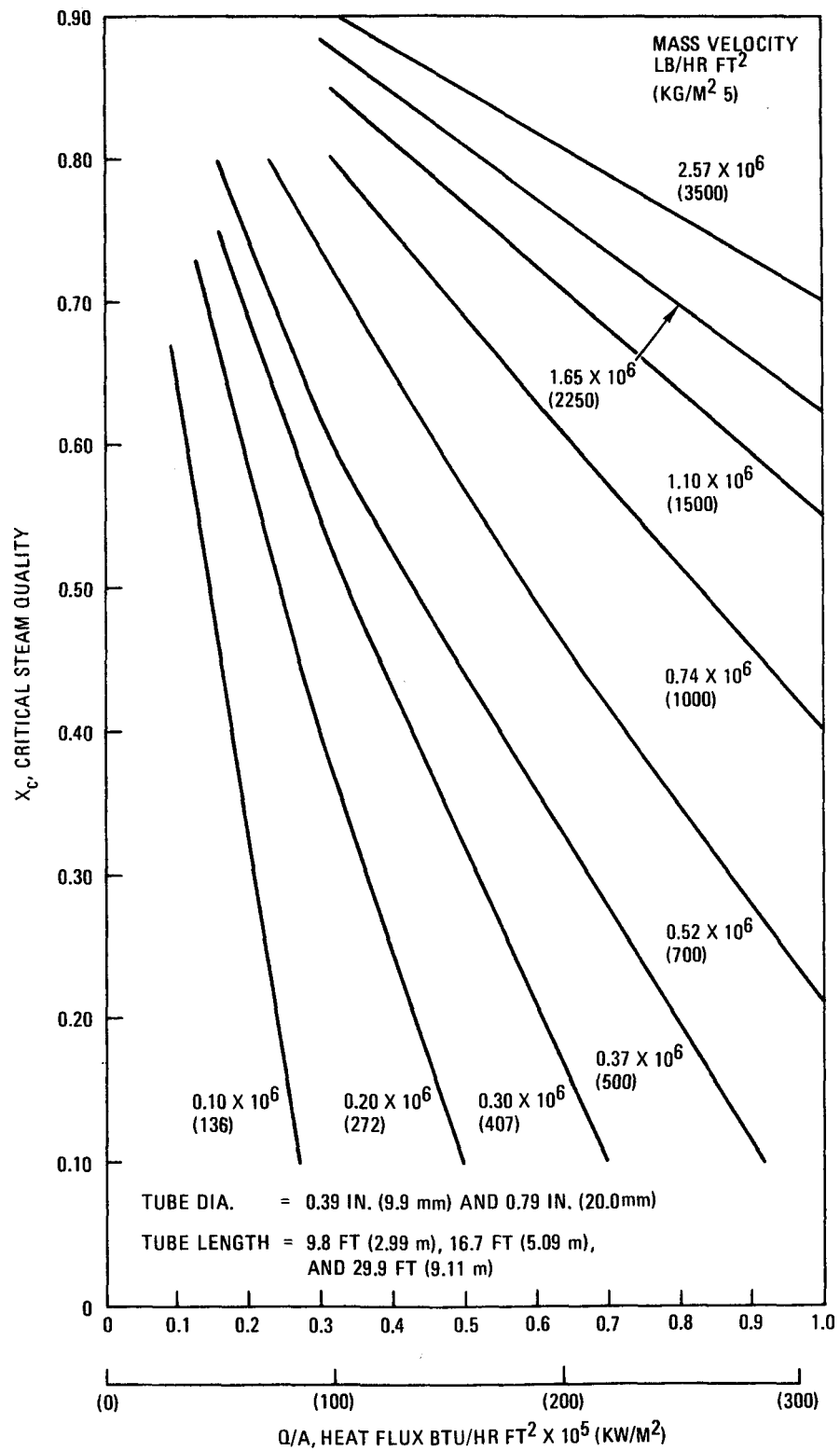


Fig. 10.2 Critical quality versus critical heat flux for steam at 2972 psia (20.5 MPa)

A measure of the effect of roughening the outside tube surface of the straight tube unit so as to obtain twice the helium heat transfer coefficient and three times the friction factor was to reduce the main bundle length by about 27%, and to increase the total helium pressure drop by 10% to 22%, depending on tube spacer free flow area.

Even though the effective temperature difference is reduced by 10%, the required length of a straight tube resuperheater is reduced to about 60% by using a four-pass steam path arrangement instead of the present single pass system. The significant size reduction results from the fact that, in the resuperheater, the steam side heat transfer coefficient is less than that of the helium side.

10.8. AUXILIARY CIRCULATOR, VALVE AND SERVICE SYSTEM

The overall objective of this task is to develop components for the core auxiliary cooling system (CACS). During this fiscal year the work is planned to generate a CACS component development plan document for the 300-MW(e) GCFR for the purpose of providing the bases for CACS component development to meet the reliability and safety criteria, and designing a test program to demonstrate the performance of the critical components by testing under anticipated operating conditions.

The scope of this task includes evaluating the development requirements for the components of the CACS for the GCFR, and therefore requires identification of the components or subcomponents that require development and the facilities that are necessary to carry out the test program.

Components included in the review and evaluation are the auxiliary circulator, its service and control system, the auxiliary heat exchanger, the auxiliary loop isolation valve, and the core auxiliary cooling water and heat dissipation system.

As part of this task, a review of the CACS for 2000/3000 MW(t) standard HTGR plants following design basis depressurization accident conditions

has been made in order to compare the requirements and evaluate proposals to meet them. The governing critical safety limit for the HTGR core cool-down by the CACS following an accident transient is the metal temperature of the support and cover plate for the thermal barrier in the lower cross duct to the core auxiliary heat exchanger (CAHE). However, the critical safety limit of the CACS for the GCFR during the accident transient is governed instead by the limiting temperature of the metallic fuel cladding 2300-2500°F (1260-1371°C).

Further GCFR safety analyses are presently being conducted under private funding in response to the DOL questions raised in their Preapplication Safety Evaluation Report (August 1974) concerning the fuel cladding temperature limit, air ingress, laminar flow distribution and the 25 in.² ($1.61 \times 10^{-2} \text{ m}^2$) depressurization leak area. The results of this analysis will be used to establish the basic criteria of the CACS and its components, including the auxiliary circulator, auxiliary heat exchanger, and the auxiliary drive motor and its control system.

An outline for the development plan has been prepared for the CACS design verification and support tasks.

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XI. HELIUM CIRCULATOR TEST FACILITY (189a No. SU046)

The objective of this task is to develop a test facility for qualification testing of the GCFR main helium circulator. Included in the scope of this task are the evaluation of alternative test facility concepts in terms of technical feasibility and cost, the identification of the most promising test facility concepts, the preparation of specifications for the selected test facility concept, an architect/engineer (A/E) preliminary design study, and final design, construction and checkout of the facility.

The first phase, including definition of the circulator test facility requirements and a feasibility scoping study, was completed at the end of FY-75 and summarized in the previous quarterly reporting period. A draft of a topical report describing the full power concepts and their evaluation is completed and being reviewed.

The FY-76 work plan and an updated Schedule 44 for the helium circulator test facility were issued. Estimated costs used in the Schedule 44 were for a closed steam loop system powered by electric motor driven steam compressors.

The Rocketdyne Division of Rockwell International has a multi-motor electric drive and gear box facility located at its Santa Susana Field Test Laboratory near Los Angeles, California. This facility, referred to as the Components Test Laboratory V (CTLV), has potential as a drive for the steam compressor in a closed loop helium circulator test facility, although the power presently available is below that desired for 100% power testing. Anticipated costs for additional equipment and refurbishing of the Rocketdyne CTLV electric motor drive system have been calculated.

Additional information has been requested describing the possible facility use arrangement and associated costs.

Work is continuing on the cost estimates for a 25% power test facility using the same system concepts as the 100% power study reported last quarter.

XII. REACTOR SAFETY (189a No. SU021)

The purpose of this task is to study the reactor safety aspects of the GCFR. Logical probabilistic methods are employed to determine the probabilities associated with various accident initiation and progression sequences and to identify potential design modifications that would help reduce risks. The thermal behavior of the fuel element duct walls under conditions of loss of shutdown heat removal is studied to determine the relative timing of duct wall melting and fuel melting. This task also includes liaison between GA and the RRD-funded GCFR Safety Task at Argonne National Laboratory (ANL).

12.1. ACCIDENT INITIATION AND PROGRESSION ANALYSIS (AIPA)

Applying the accident initiation progression analysis (AIPA) techniques developed in FY-74, work is being directed toward the probabilistic analysis of potential accident sequences leading to low-probability, high-consequence sequences of events (also under study at ANL on "GCFR Safety Aspects of Fuel and Core"). Figure 12.1 shows schematically the principal factors that are expected to determine the magnitude of the consequences for the high consequence accidents under investigation. These parameters are (1) plant size, (2) accident type or class, (3) coolant condition at the time of onset of core damage (pressurized or depressurized), and (4) the key accident parameter or parameters that characterize each accident class. As shown, the principal AIPA effort is directed at the 300-MW(e) demonstration plant, with scoping type analyses to be performed for larger plant sizes. The three accident classes identified are loss of flow (LOF) with shutdown, loss of flow (LOF) with failure to shutdown, and transient overpower (TOP). It is the objective of the AIPA work to assign probabilities to the left-hand branches (accident variables) of

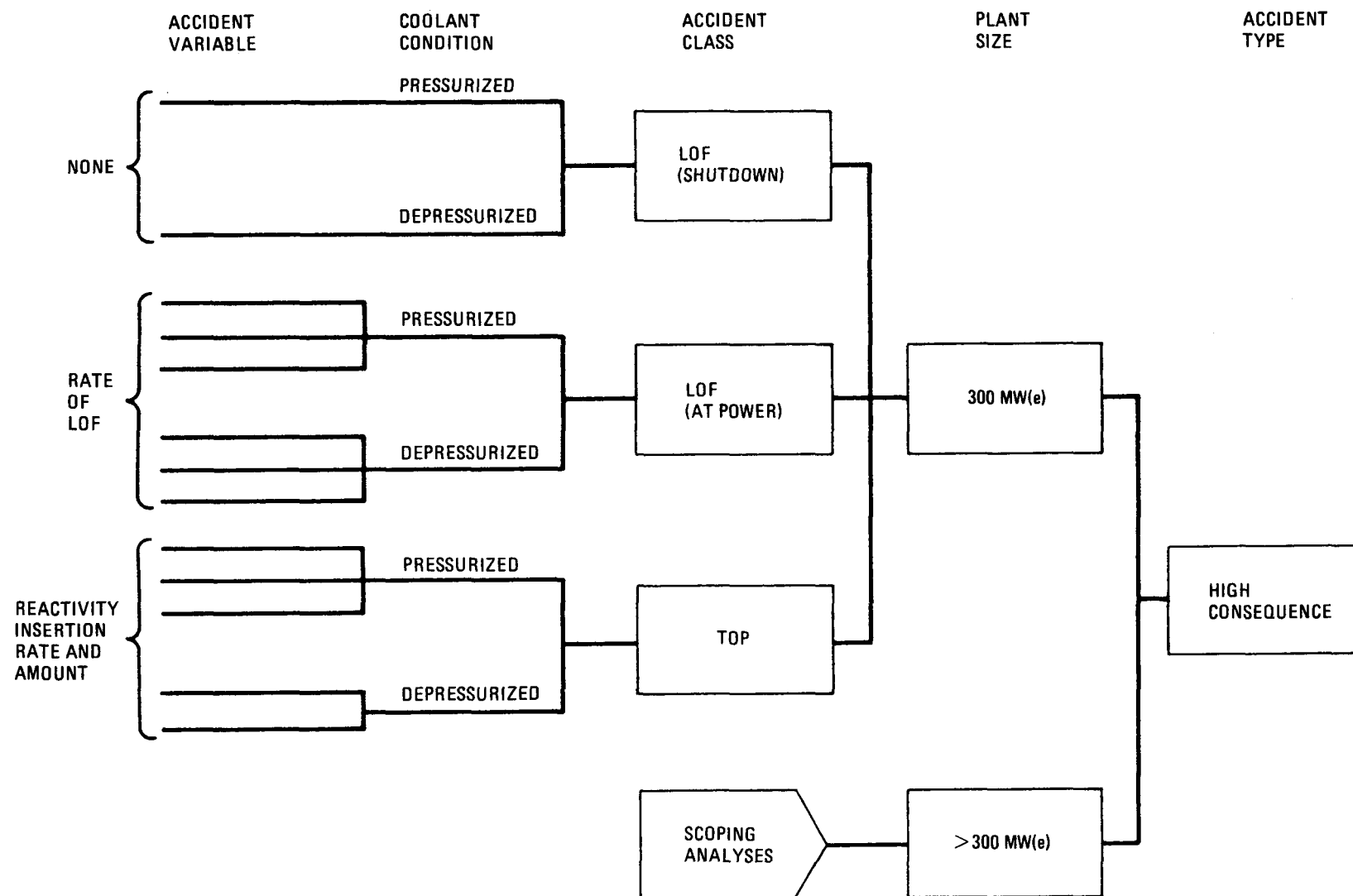


Fig. 12.1 GCFR-AIPA subtask interface with ANL safety work

Fig. 12.1 by bunching the various accident event sequence paths into the corresponding consequence similarity group.

During this reporting period, work was completed on a first-cut estimation of sequences leading to a loss of flow in a shutdown reactor. As shown in Table 12.1, this work has identified four major initiating event groups for each of the two consequence groupings shown for the LOF-with-shutdown class of accident in Fig. 12.1. The groupings selected include consideration of both internal initiators (plant hardware failures) and external initiators (loss of offsite power, major earthquake). For the pressurized case the event groups are

1. Innocuous Transients. Events leading to the shutdown of the operating reactor which, of themselves, do not cause failure of the hardware necessary to maintain shutdown core cooling (i.e. inadvertent reactor trips, training shutdowns, etc.).
2. Loss of Single Main Loop. Failures which require the isolation of a single main loop (i.e. small steam generator leaks, helium circulator failures, feed regulation failures, etc.) but do not of themselves cause failure of components in the other main loops.
3. Common Loss of Main Loops. Failures that commonly affect the normal operation of all three main loops (failure of service water systems, failure of control air, loss of feedwater supply, etc.).

For the depressurized case the event groups are

1. Piping Failures. Failures of piping penetrating the primary coolant system leading to a slow depressurization of the PCRV.
2. Seal Failures. Failures of major PCRV penetration closures leading to a design basis depressurization accident.

Table 12.1
SUMMARY OF LOF WITH SHUTDOWN ACCIDENT COOLING FAILURE PROBABILITIES
(Preliminary)

Event Group	Assumed Frequency (Events Per Year)	Total Cooling Failure Probability (Failures Per Year)
Pressurized		
Innocuous Transients	3	2×10^{-6}
Loss of Single Main Loop	1	5×10^{-7}
Common Loss of Main Loops	3×10^{-4}	9×10^{-8}
Loss of Offsite Power	1×10^{-1}	5×10^{-6}
Depressurized		
Piping Failures	3×10^{-5}	$\epsilon(2 \times 10^{-11})$
Seal Failures	1×10^{-7}	$\epsilon(3 \times 10^{-12})$
Major Steam Leak	3×10^{-3}	$\epsilon(7 \times 10^{-12})$
Major Earthquake (>SSE)	1×10^{-7}	$< 10^{-9a}$

^aBased on preliminary work performed under the HTGR-AIPA Task.

3. Major Steam Leak. Major failure of steam piping within the PCRV with the potential for leading to a primary system overpressurization followed by system depressurization through relief valves that fail to reseal.
4. Major Earthquake. Earthquake of a magnitude greater than the safe shutdown earthquake (SSE).

Table 12.1 summarizes the failure probabilities calculated for each of these events. These failure probabilities include considerations of component random failures, test and maintenance unavailabilities, and common mode failures. The common mode failure approach utilized was developed under the HTGR-AIPA program. As shown in Table 12.1, preliminary estimates

of the total cooling failure probability for the various event groups indicate that the cumulative probability of a loss of cooling event with shutdown may be in the range of 10^{-5} to 10^{-6} per year for the pressurized case, and orders of magnitude less in the depressurized case. The estimate of cooling failure sequence probabilities in the depressurized case is so low (as indicated by the epsilon (ϵ) in Table 12.1) that it is expected that there may be other accident sequence paths leading to the same outcome (such as those originating from a major earthquake) of more significance. The probabilities shown for the major earthquake event are very tentative and are based on preliminary work performed under the HTGR-AIPA task.

As indicated in Table 12.1, the loss-of-offsite-power initiating event has been identified as having the highest probability of leading to a loss of flow with shutdown accident. Figure 12.2 shows the dominant sequence leading to cooling failure for this initiating event group and the failure probabilities that have been assessed for each major sequence event. The dominant sequence involves a total loss of all offsite electrical power with a subsequent failure of the main turbine generator control system to catch the plant's in-house electrical loads, leading to a loss of normal electrical power supplies. The loss of condenser vacuum and of essential electrical bus voltage generates signals to cause a reactor trip and start the emergency diesel generators. Even if all three diesel generators fail to start, shutdown cooling can continue, using the main loops, in excess of fifteen minutes on the stored inventory in the steam generators. Following this period, a single core auxiliary cooling loop can provide adequate core cooling. However, if offsite or onsite electrical power cannot be restored, core overheating would occur. As shown, the probability of this sequence has received a preliminary estimate of 4×10^{-6} per year.

12.2. SAFETY RELATED ANALYTICAL AND EXPERIMENTAL STUDIES

During this quarterly reporting period, a detailed model of a 1/12-sector of a GCFR fuel element has been developed using the thermal network analysis code SINDA⁽¹⁾ (Systems Improved Numerical Differencing Analyzer).

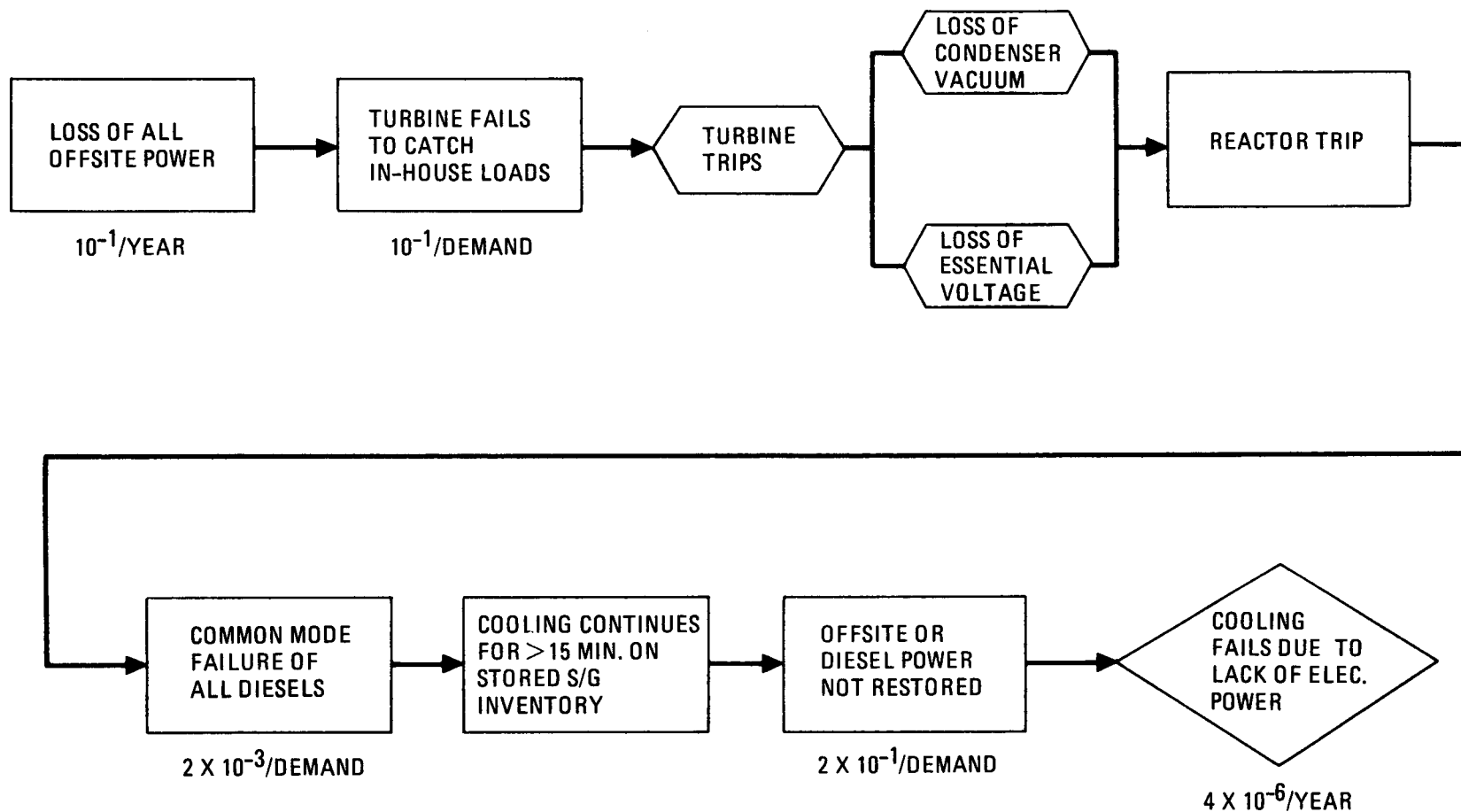


Fig. 12.2 Dominant accident sequence for the loss of offsite power event group

This model has been developed in order to more realistically determine the thermal behavior of fuel elements and duct walls under conditions of loss of shutdown heat removal, and to assess the accuracy of predictions using the simpler BOXRAD model.

The model of the 1/12-sector and of a fuel rod are shown in Fig. 12.3. In this model, each fuel rod is represented by six fuel segments surrounded by six cladding segments. Within a fuel rod, the following heat exchange processes are modeled:

1. Circumferential and radial conduction heat transfer between adjacent solid nodes, including a fuel-to-cladding gap/resistance, and
2. Radial heat transfer across the central fuel hole in rods where a significant temperature gradient exists.

From the fuel rod surface or from the duct wall nodes, the following heat transfer mechanisms are included:

1. Conduction heat transfer into the adjacent coolant node, and
2. Radiation heat transfer to the surrounding solid surface, including the duct wall where appropriate.

Additional heat transfer paths included are

1. Conduction heat transfer between adjacent duct wall nodes,
2. Conduction heat transfer between adjacent coolant nodes, and
3. A simulation of heat losses due to convective heat transfer in the coolant channel.

The effect of melting steel components (cladding, duct wall), in the model is accomplished by accounting for the heat of fusion during the melting

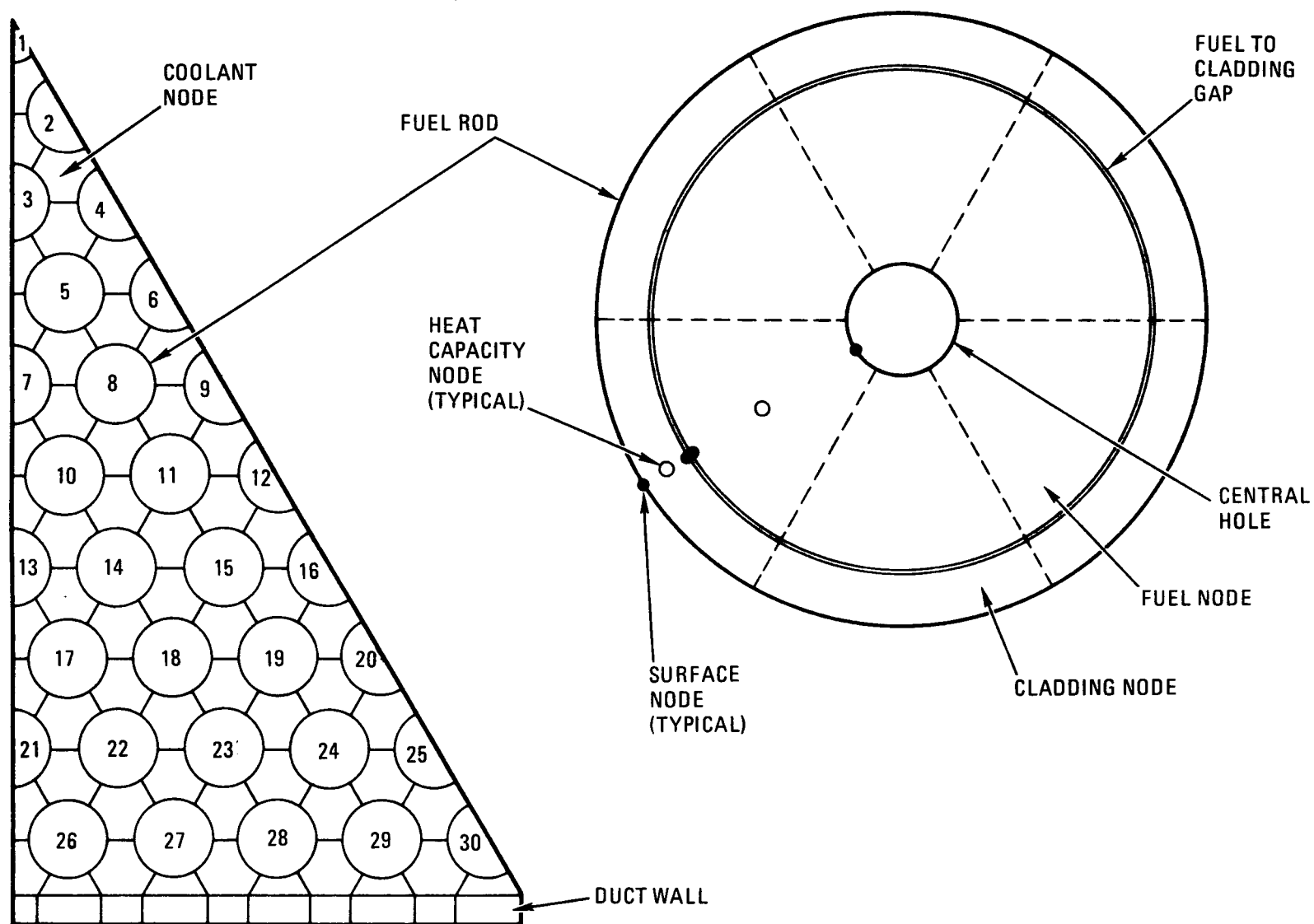


Fig. 12.3 Thermal model of a 1/12-sector of a fuel element

process and by changing the thermal properties of a melted node to those of helium when melting has been completed. The melting criterion can be specified as any temperature or fractional addition of the heat of fusion. Adjustments are also made in the radiation view factors due to the geometry changes that occur when a steel node melts.

The SINDA model is now operating and is being tested. Initial comparison to analyses with the much simpler BOXRAD model are very encouraging.

12.3. SAFETY RESEARCH AND DEVELOPMENT LIAISON

A GCFR Safety Program Review Meeting was held September 11-12, 1975 at Argonne National Laboratory with participation from ERDA-RRD, ANL, GA, ANC, HNL, EPRI, UCLA, Rand Corp. and Northwestern University. Work progress was reported by ANL and GA.

Additional GCFR design documents were provided to ANL upon their request.

REFERENCE

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XIII. IN-PILE SAFETY TEST PROGRAM-GRIST (189a No. SU015)

13.1. INTRODUCTION

The Gas Reactor In-Pile Safety Test (GRIST) program is being evaluated by ERDA-RRD, Argonne National Laboratory (ANL) and General Atomic (GA) as a potential follow-on to analytical and experimental programs being conducted under the LMFBR and GCFR programs. Essential data for the design basis of the GCFR demonstration plant would be provided by in-pile endurance testing of the 12-rod GCFR test assemblies in the BR-2 in Mol, Belgium, and by out-of-pile tests with the core flow test loop (CFTL) described in Section IV. The GRIST program has the objective of going beyond the design basis accidents, and in particular investigating the behavior of melted cladding and fuel. However, testing of GCFR type fuel assemblies in the GRIST program may include less severe transients of core power, coolant flow, and coolant pressure in order to investigate the effects of upset, emergency, and faulted conditions on the performance of fuel assemblies for gas-cooled fast breeder reactors. These tests would provide information that is projected to be of importance for improving the performance of commercial GCFR plants.

The studies conducted in FY-75 included: (1) a comprehensive review of potential program objectives, (2) design trade-off studies, (3) schedule and cost estimates for the in-pile loop facility to be installed in the Engineering Test Reactor at Idaho National Engineering Laboratory (INEL), and (4) a conceptual design study for a reference test bundle.

During this quarterly period, efforts were devoted to the completion of a conceptual design being proposed for a prototypical 37-rod fuel assembly to be tested in the GRIST facility. A scoping analysis of test

size requirements for duct wall melting tests was initiated. The test assemblies will be designed to prototypically simulate transient temperature behavior of the duct wall. The test assemblies being considered include sections of two and three adjacent fuel assemblies and sections of control assemblies. The preparation of thermal-hydraulic analyses to support the design work was initiated.

13.2. TEST ASSEMBLY DESIGN

The conceptual design of a 37-rod assembly, which is prototypical of GCFR fuel assemblies, has been completed. The design drawings have been sent to program participants together with a design description. Key features of this conceptual 37-rod assembly design have been described in the previous quarterly.

The design effort during this quarterly period was focused on a clear description of the interface between the test train and the test assembly, which has been proposed to be located at the handling socket. The result of this effort is shown in Fig. 13.1. The handling socket with the instrument lead connectors continues to be considered a practical location for the interface.

13.3. TEST ASSEMBLY ANALYSIS

In areas where the GRIST test designs are similar to the GCFR fuel assemblies, the analytical tools and results that have become available under the GCFR fuel element development are being applied. Included are the thermal-hydraulic considerations that go into the definition of the rod bundle geometry, the spacer design, and the pressure drop calculations. The main analytical methods being used are those included in the COBRA code.

Only in areas where the GRIST tests are different from GCFR fuel assemblies are specific analyses being conducted. For example, in the

NOTES

- 1 DIMENSIONS DEFINING CONICAL SURFACE TO BE SUPPLIED BY GENERAL ATOMIC
- 2 INTERNAL DIMENSIONS DEFINING LOCATION OF MATING PARTS AND SURFACES TO BE SUPPLIED BY GENERAL ATOMIC.

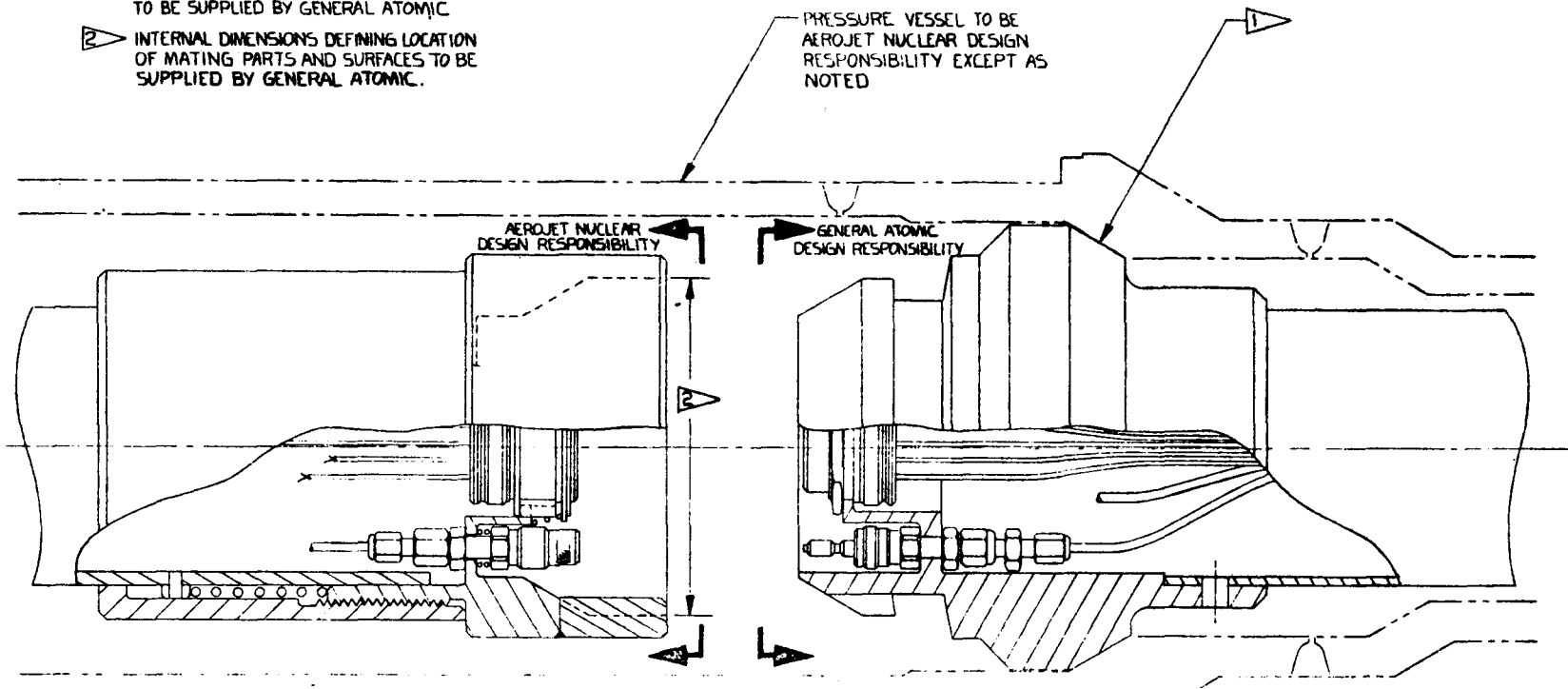


Fig. 13.1 Handling socket and test train/test assembly interface

GRIST facility, at the test section outlet the main coolant has to be mixed with relatively cold helium to keep the outlet temperature below 399°C (750°F). For this attemperation flow, only limited space is available. The current conceptual design for the 37-rod bundle test provides an annular gap with an equivalent hydraulic diameter of 10 mm. The design requirement for the gap width is deduced from pressure drop considerations: the pressure drop along the annular gap must not exceed the test section pressure drop. A preliminary analysis was performed to study the sensitivity of the pressure drop as a function of flow and gap width at the current design point. The results, shown in Fig. 13.2, indicate that the pressure drop for a hydraulic diameter of 10 mm is most sensitive to the gap width. To ease flow control problems for loop operation, it was recommended that based on these results an increase of the hydraulic diameter to 12.5 or 15 mm be considered.

For the 300-MW(e) GCFR demonstration plant, changes are being considered in the fuel assembly design. These considerations indicate that the fuel rod diameter will remain at 7.2 mm; however, the rod pitch may change from 10.0 mm to 10.8 mm. Based on this 8% increase of the rod pitch the space required for the GCFR fuel assembly testing was reevaluated. The results are shown in Table 13.1. The test diameter requirements for a 37-rod assembly would increase from 105 mm to 111.4 mm, whereas the requirements for a 61-rod assembly would increase from 125.0 mm to 133.0 mm.

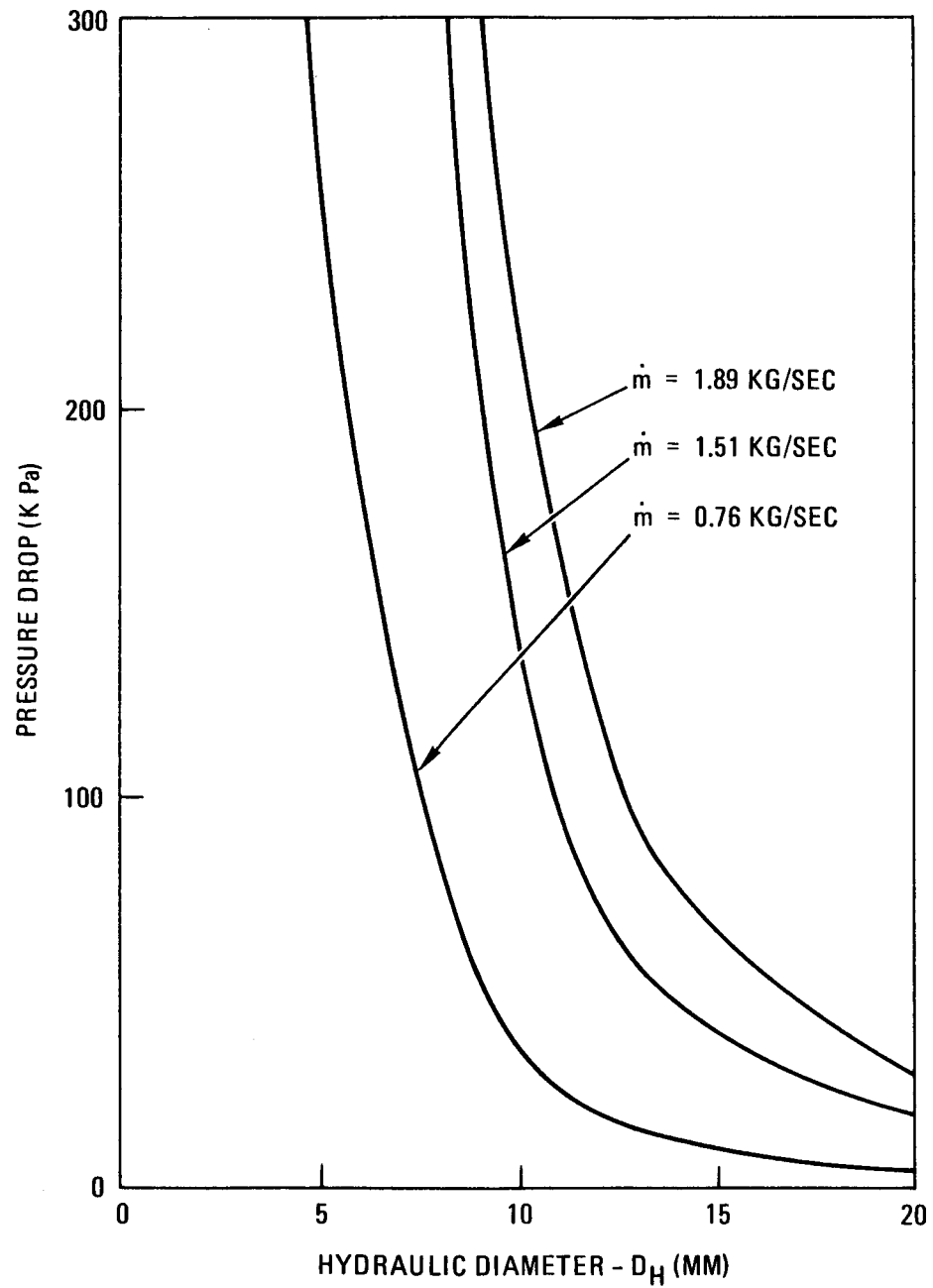


Fig. 13.2 Pressure drop of the attemperation flow in the test region as a function of the hydraulic diameter

Table 13.1
TEST ASSEMBLY REQUIREMENTS

Number of Rods		Total Test Diameter Requirements (mm) ^a	
Rod Centered Configuration	Coolant Subchannel Configuration	10 mm Rod Pitch	10.8 mm Rod Pitch
--	3	55	57.4
7	--	65	68.2
--	12	75	79.0
19	--	85	89.8
--	27	95	100.6
37	--	105	111.4
--	48	115	122.2
61	--	125	133.0

^aGross in-pile tube (through tube) inner diameter requirements including space for: (1) a second duct wall for thermal insulation (10 mm on the diameter), and (2) attemperation flow area (15 mm on the diameter).