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GAS-COOLED FAST BREEDER REACTOR

**QUARTERLY PROGRESS REPORT FOR THE PERIOD
FEBRUARY 1, 1978 THROUGH APRIL 30, 1978**

by
PROJECT STAFF

EX-101

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ABSTRACT

The tasks of the gas-cooled fast breeder reactor (GCFR) program which are supported by the Department of Energy include development of GCFR fuel, blanket, and control assemblies; development of the pressure equalization system for GCFR fuel; out-of-pile loop facility test programs; 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 the core support structure; plant systems control; systems engineering; development of reactor components, including reactor vessel, control and locking mechanisms, fuel handling equipment, core support structure, shielding assemblies, main helium circulator, steam generator, circulator test facility, and auxiliary circulator; development of a helium circulator test facility; reactor safety, environment, and risk analyses, including planning and support of an in-pile and out-of-pile safety test program; nuclear island engineering design; and development of a reliability data bank.

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1. INTRODUCTION

The various tasks of the gas-cooled fast breeder reactor (GCFR) program for the period February 1, 1978 through April 30, 1978 sponsored by the Department of Energy (DOE) are discussed in this quarterly progress report. The GCFR utility program, which is sponsored by a large number of electric utility companies, rural electric cooperatives, and General Atomic (GA), is primarily directed toward the development of a GCFR demonstration plant. The utility-sponsored work and the DOE-sponsored work are complementary.

Analytical, experimental, and fabrication development is being accomplished under the core assembly development task to establish the basis for the design of GCFR fuel, blanket, and control assemblies. Methods development for structural, thermal-hydraulic, and mechanical analyses is discussed, and the results of structural analysis of the fuel assembly components and thermal-hydraulic analysis of the blanket assembly during low power are presented. Current progress on rod-spacer interaction tests, fuel assembly seismic and vibration test planning, and development of assembly fabrication techniques is also presented. The various subtasks of core assembly development and the work accomplished during this reporting period are discussed in Section 2.

The technology to support the design and construction of the pressure equalization system (PES) for GCFR fuel is being developed. This includes (1) the development of analytical models and computer codes which will be verified by test programs and testing of materials and seals and (2) the development of fabrication processes for the PES. These are discussed in Section 3.

To demonstrate the ability of GCFR fuel, control, and blanket assembly designs to meet design goals and verify predictions of analytical models, a

series of out-of-pile simulation tests will be performed. The emphasis of the tests will be on obtaining thermal-structural data for steady-state, transient, and margin conditions using electrically heated rod bundles in a dynamic helium loop. These are discussed in Section 4.

In the fuels and materials development program, thermal flux and fast flux irradiation studies 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, an irradiation test program of smooth and surface-roughened GCFR cladding specimens is being conducted to determine how these materials behave under irradiation. The fuels and materials tests, the analytical studies, and the results to date are presented in Section 5.

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 6.

The objectives of the nuclear analysis and reactor physics task are to verify and validate the nuclear design methods which will be applied to the GCFR core design. Data from a critical assembly experimental program at the ZPR-9 facility at Argonne National Laboratory (ANL) are being used for this purpose. Critical assembly design, analysis, and methods development are discussed in Section 7.

Verification of the physics and engineering analytical methods and the data for design of the GCFR shields is being conducted under the shielding requirements task along with an evaluation of the effectiveness of various shield configurations. The results of radial shield analyses and the work being done on structural analysis are presented in Section 8.

Section 9 discusses systems engineering for the GCFR. This includes systems integration; coordination of interface requirements between plant systems; and development and implementation of effective documentation management.

Section 10 discusses the evaluation and development of the main components of the GCFR, including reactor vessel, control and locking mechanisms, fuel handling, core support structure, shielding assemblies, main helium circulator, steam generator, auxiliary circulator, and helium processing components. Section 11 is concerned with the engineering required to design and develop the circulator test facility, and Section 12 reports on the development of control systems and the assessment of seismically induced and flow-induced vibration behavior for the GCFR demonstration plant.

The reactor safety task, which is discussed in Section 13, includes (1) maintenance of liaison between GA and other organizations and integration of the overall GCFR safety analysis effort; (2) formulation and review of the GCFR safety program plan; (3) performance of detailed safety, environmental, and risk analyses of the GCFR; (4) evaluation of the post-accident fuel containment (PAFC) capability of the GCFR; (5) integration of the results of DOE safety studies into the licensing reviews; and (6) evaluation of probabilistic design methods for use in the GCFR program. Procurement, supply, and storage of reliability data are also reported along with estimates in support of probabilistic analyses of accident events being analyzed for gas-cooled reactors.

Section 14 discusses the safety test program, which involves quantification of fuel and cladding behavior during accidents leading to core damage and identification of safety test information required for licensing and commercialization of the GCFR. The GRIST-2 and duct melting and fall-away test programs (DMFT) are also examined.

Section 15 discusses the nuclear island. The purposes of this task are to accomplish engineering design work on the nuclear island portion

of the demonstration plant and to resolve the interface requirements of major nuclear steam supply (NSSS) and balance of plant (BOP) systems.

Development of an alternate design concept for the NSSS and related BOP facilities and equipment is discussed in Section 16, and the characteristics of a GCFR fueled with combinations of U-233, U-235, U-238, plutonium, and thorium are reported in Section 17.

2. CORE ASSEMBLY DEVELOPMENT (189a No. 00582)

2.1. CORE ASSEMBLY THERMAL-HYDRAULIC ANALYSIS

2.1.1. Introduction

Experimental data are being evaluated to develop the analytical basis for the design and development of the GCFR fuel, control, and blanket assemblies. 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 effort is devoted to the development of an adequate steady-state and transient analysis capability in the areas of thermal-hydraulic and structural analyses to provide a basis for assembly design criteria and specific test requirements. The main efforts have focused on improvement of thermal-hydraulic correlations and development of methods for applying the correlations to the design and analysis of GCFR core assemblies.

2.1.2. Fuel Assembly Analysis

2.1.2.1. Sizing Studies for the Fuel Assembly Central Rod. Preliminary analysis has been performed utilizing the COBRASIDE-2 (Ref. 2-1) code for sizing the fuel assembly central rod; an unfueled instrumentation tube is located in this position. Since the studies were parametric in nature, the central rod region and corner rod and edge spacing region were separately treated at first to greatly reduce the amount of subchannels modeled and consequently the computer running time (Fig. 2-1). The results show that the circumferential thermal gradient on the fuel rods adjacent to the central rod can be minimized to within 14°C. This 14°C gradient includes the approximate Nusselt correction to accommodate the existence of the central rod, which is larger than the fuel rods. Decreasing the size of

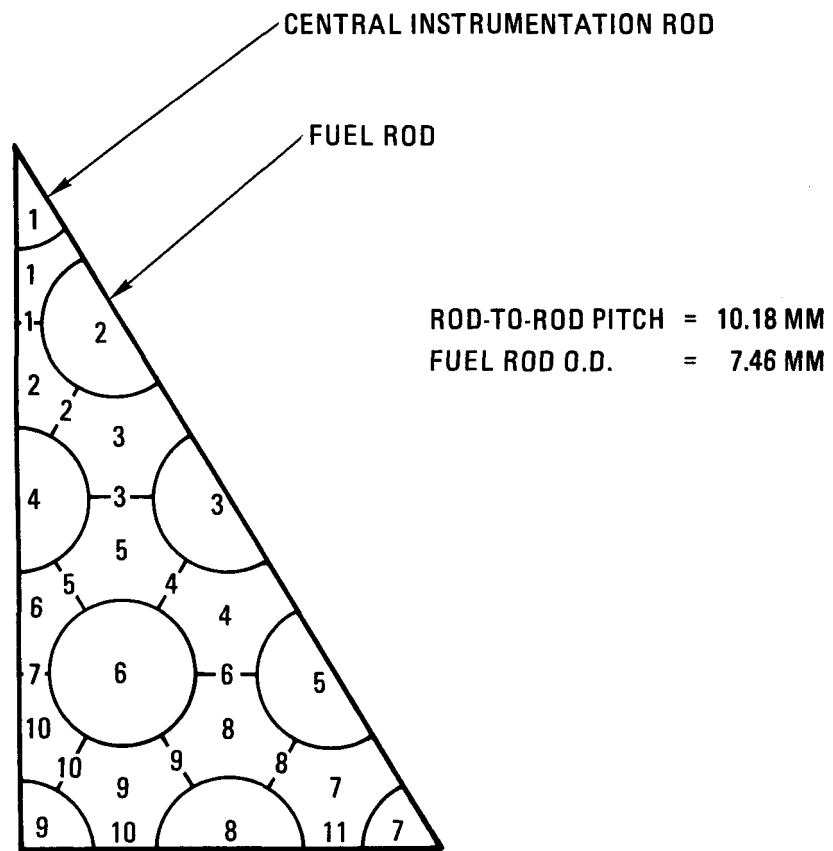


Fig. 2-1. COBRA model for central instrumentation rod region study

the central rod upstream smooth portion can reduce the 14°C thermal gradient to within 10°C, but at the expense of significantly increasing the gradient at the start of the roughened portion to about 10°C or higher. In addition, all the gradients at the downstream portion of the fuel rod would be increased several degrees. As a result of this analysis, the following conclusions have been reached:

1. The central rod should be roughened in the same manner as the fuel rods.
2. The central rod should have a volumetric average diameter of 9.5 mm for a rod-to-rod pitch of 10.18 mm and a fuel rod volumetric average diameter of 7.28 mm.
3. The central rod diameter size above gives the adjacent fuel rods a maximum circumferential thermal gradient of about 6°C in the roughened portion (inward direction) and 14°C in the smooth portion (outward).

2.1.2.2. Edge Spacing Studies. The edge spacing analysis reported in Ref. 2-2 was repeated for a 10-18-mm-pitch fuel assembly design. A 27-subchannel, 14-rod region of a fuel assembly was modeled (Fig. 2-2). Since the edge analysis of the corner rod has not yet been performed, meaningful results come from the wall region away from the corner rod (e.g., subchannels 25, 26, 27; fuel rods 13 and 14). It was assumed that subchannel 23 has the same type of hydraulic characteristics as the other wall portions of the wall channels, including the spacer loss coefficient (to minimize the corner rod region effects).

Parametric variations of the edge spacing dimension were performed to determine the optimum edge spacing size needed to minimize the circumferential thermal gradient across the edge fuel rods. An optimum edge spacing of 1.45 mm is consistent with previous results obtained for an 11.05-mm-pitch core (Ref. 2-2). A maximum circumferential fuel rod thermal

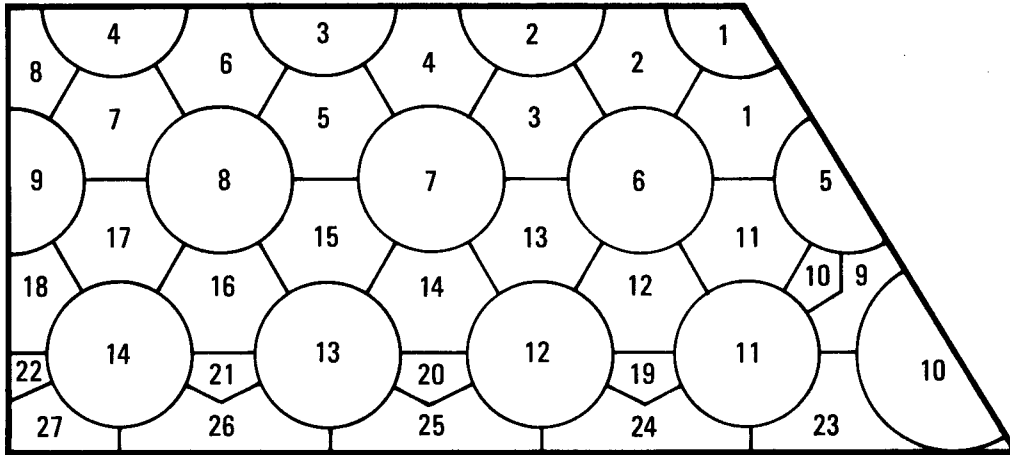


Fig. 2-2. COBRA model for edge spacing study

gradient of 34°C is a rather large value compared with the corresponding value obtained in the central rod sizing study (Section 2.1.2.1). However, an outward thermal gradient of 34°C exists in the roughened region and is expected to be reduced during laminar flow operations.

This study, which resulted in a 50% edge spacing, confirmed previous calculations which resulted in a 52% edge spacing. An edge spacing larger than 50% slightly increases edge channel overcooling during normal operation and reduces edge channel undercooling during laminar flow. Since a new reference assembly design is being defined, no edge spacing change is recommended at this time. The optimum edge spacing was determined to be 1.45 mm, which is equal to 50% of the nominal gap width between fuel rods. The axial distribution of the circumferential surface temperature of a typical fuel rod (rod 13) is presented in Table 2-1. This table shows that there is a maximum inward thermal gradient of 34°C at axial location 11. This inward gradient persists through axial location 14 and then switches to the outward direction. At axial location 20, there is a maximum outward thermal gradient of 34°C. This switching of directions of thermal gradients along the fuel rod is caused by the change from a smooth to a roughened surface, hence creating different hydraulic behavior for different portions of the flow channel.

2.1.2.3. Natural Convection Scoping Study for an Upflow GCFR Core.

Feasibility studies of an upflow GCFR are currently in progress. It is also of interest to study the detailed thermal-hydraulic behavior of a fuel assembly for an up-flow core under natural convection conditions. Preliminary findings are presented of a scoping study of a system with an elevation difference of about 7.6 m between core and auxiliary heat exchangers, which provides a natural convection flow rate of about 13.86 kg/s through the core. The analysis employed the steady-state code SCEPTIC (Ref. 2-3). The input conditions were taken from the system transient study (Ref. 2-4) with the cladding at its maximum temperature (notice that the system study does not model a fuel assembly in detail). The central hottest fuel assembly was modeled for the analysis. Some key input data are listed in Table 2-2.

TABLE 2-1
AXIAL DISTRIBUTION OF THE CIRCUMFERENTIAL SURFACE TEMPERATURE OF FUEL ROD NO. 13

Axial Location No.	Axial Distance From Inlet (cm)		Coolant and Rod Surface Temperature (°C) (a)								
			T _f (25) ^(b)	T _f (26)	T(14) ^(c)	T(15)	T(16)	T(20)	T(21)	T(25)	T(26)
1	0.0	Smooth ↑	316	316	316	316	316	316	316	316	316
2	9.4		316	316	316	316	316	316	316	316	316
3	18.5		316	316	316	316	316	316	316	316	316
4	27.9		316	316	317	317	317	317	317	317	317
5	37.1		316	316	320	320	320	320	320	320	320
6	46.5		317	317	323	323	323	324	324	324	324
7	55.9		318	318	328	328	328	329	329	329	329
8	65.0	↓ Smooth Rough ↑	319	319	400	399	400	406	406	415	415
9	74.4		335	335	471	470	471	484	484	491	491
10	83.6		353	353	514	512	514	529	529	532	532
11	93.0		374	374	452	451	452	461	461	485	485
12	102.1		398	398	485	484	485	492	492	500	500
13	111.5		419	419	515	514	515	517	517	526	526
14	120.9		441	441	543	542	543	542	542	550	550
15	130.0	463	463	570	569	570	567	567	562	563	
16	139.4	484	484	591	591	591	585	585	583	583	
17	148.6	505	505	609	609	609	597	597	596	597	
18	158.0	523	524	623	624	623	609	609	600	601	
19	167.1	540	541	631	633	631	619	619	608	609	
20	176.5	↓ Rough	554	555	620	623	620	601	602	589	590
21	185.9	Smooth	558	559	600	603	600	584	584	567	568
22	195.1	↑	560	561	595	600	595	576	576	567	568
23	204.5	561	563	590	596	590	571	573	565	567	
24	213.6	↓	562	564	588	594	588	572	573	564	566
25	223.0	Smooth	563	565	586	592	586	571	573	563	565

(a) Temperature rounded to nearest integer.

(b) $T_f(i)$ = coolant temperature in subchannel i .

(c) $T(i)$ = circumferential surface temperature of fuel rod facing i^{th} subchannel (see Fig. 2-1 for subchannel denotation).

TABLE 2-2
INPUT FOR SCEPTIC UP-FLOW CORE NATURAL CONVECTION STUDY(a)

Core inlet temperature	260°C
Total flow rate	13.86 kg/s
Decay power level	40 MW
Primary system pressure	8.82 MPa
Fraction of power to fuel assembly	0.9%
Fraction of flow to fuel assembly	0.8%

(a) Fuel assembly model was based on a 289-kPa pressure drop core.

Table 2-3 compares the results of this up-flow core natural convection study with those obtained for the down-flow core [design basis depressurization accident (DBDA)] (Ref. 2-4). Both cases were modeled for a 289-kPa pressure drop core. The results indicate that there is only a modest variance between the two cases and that the flow and temperature distributions within the assembly are independent of the flow direction through the core. Thus, under the flow conditions required for satisfactory decay heat removal, there seems to be no significant difference between the flow and temperature distributions of a forced down-flow and a natural convection up-flow core.

2.1.2.4. Thermal Analysis of Fuel Assembly Dropped During Refueling.

During the GCFR refueling operation, the core and blanket assemblies must be individually handled and moved from one location to another. Typically, a fuel transfer machine removes an assembly from the core and delivers it to a fuel lifting machine, which passes it through a PCRV penetration into a fuel transfer cask (Ref. 2-5). During these handling and transfer operations, the machines provide forced helium coolant flow through the assemblies. It is conceivable that an assembly could be dropped to the cavity floor and have to be retrieved by the machines. There would be no internal coolant flow while the assembly is separated from the machines and the assembly internal temperature would rise, causing the afterheat to be removed. The extent of the temperature rise within the assembly will determine the provisions necessary for preventing fuel rod melting.

The purpose of this investigation was to predict the internal temperatures of a dropped fuel assembly as a function of various assumed external conditions and to provide recommendations for reducing the probability of cladding melting. The latest energy deposition rate data and the current fuel assembly geometry have been input to the SCEPTIC thermal-hydraulic analysis code, which is capable of simultaneous handling of radiative and convective heat transfer (Ref. 2-3). The principle assumption of the analysis is that the assembly retains its structural configuration after

TABLE 2-3
COMPARISON OF KEY RESULTS OF NATURAL CONVECTION UP-FLOW
AND DBDA FORCED-CONVECTION DOWN-FLOW CASES

Parameter	Up-Flow Natural Convection Case	Down-flow DBDA Forced-Convection Case
Maximum thermal gradient across outer edge fuel rod (°C)	82	67
Maximum temperature dif- ference between edge and interior fuel rods (°C)	202	203
Total power of the 1/12- sector model of fuel assembly (W)	30,000	23,380
Average Reynolds number corresponding to maximum thermal gradient location	1,350	1,100

impact on the preshield. Obviously, significant rearrangement of the fuel rods would have a marked effect on local temperatures.

As shown in Fig. 2-3, the GCFR fuel assembly modeled by SCEPTIC assumed a plane of symmetry along the axis of the bundle, so that a total of 140 full or partial rods was included. Only representative rods are shown in Fig. 2-3. Each duct flat was separated into nodes which could receive heat from the interior and transfer heat to the exterior by radiation and convection. Two fuel assembly orientations were analyzed. In the first (horizontal) configuration, the assembly is lying flat on the preshield; in the second, the bundle is leaning against the thermal shield wall (Fig. 2-4). For the horizontal configuration it was conservatively assumed that the duct flat resting on the preshield formed an adiabatic wall, preventing any convective or radiative heat loss in the downward direction. When the assembly leans against the thermal shield, heat transfer occurs on all external surfaces.

Radiation was assumed to be the dominant mode of heat transfer inside the duct; no benefit for natural convection was taken, even for the leaning configuration. Since SCEPTIC is a flow analysis code, it was necessary to provide some flow through the interior of the bundle. However, by using a small value for the Stanton number, heat transfer by convection within the bundle was reduced to a negligible level. Each rod in the assembly was designated as a separate surface, and techniques from Ref. 2-6 were used to calculate radiation view factors between the rods and from the rods to the duct wall. On the exterior, the walls were allowed to radiate to a reference sink temperature, and the coolant flow rate on each surface could be specified to establish the local heat transfer coefficient.

Two sets of external conditions were analyzed with the assembly in the horizontal configuration. One corresponds to a typically low Nusselt number for natural convection, resulting in a film coefficient of $12 \text{ W/m}^2\text{-}^\circ\text{C}$ on the five exposed flats. The other assumes a low level of forced coolant flow over the surfaces, raising the coefficient to $75 \text{ W/m}^2\text{-}^\circ\text{C}$. With

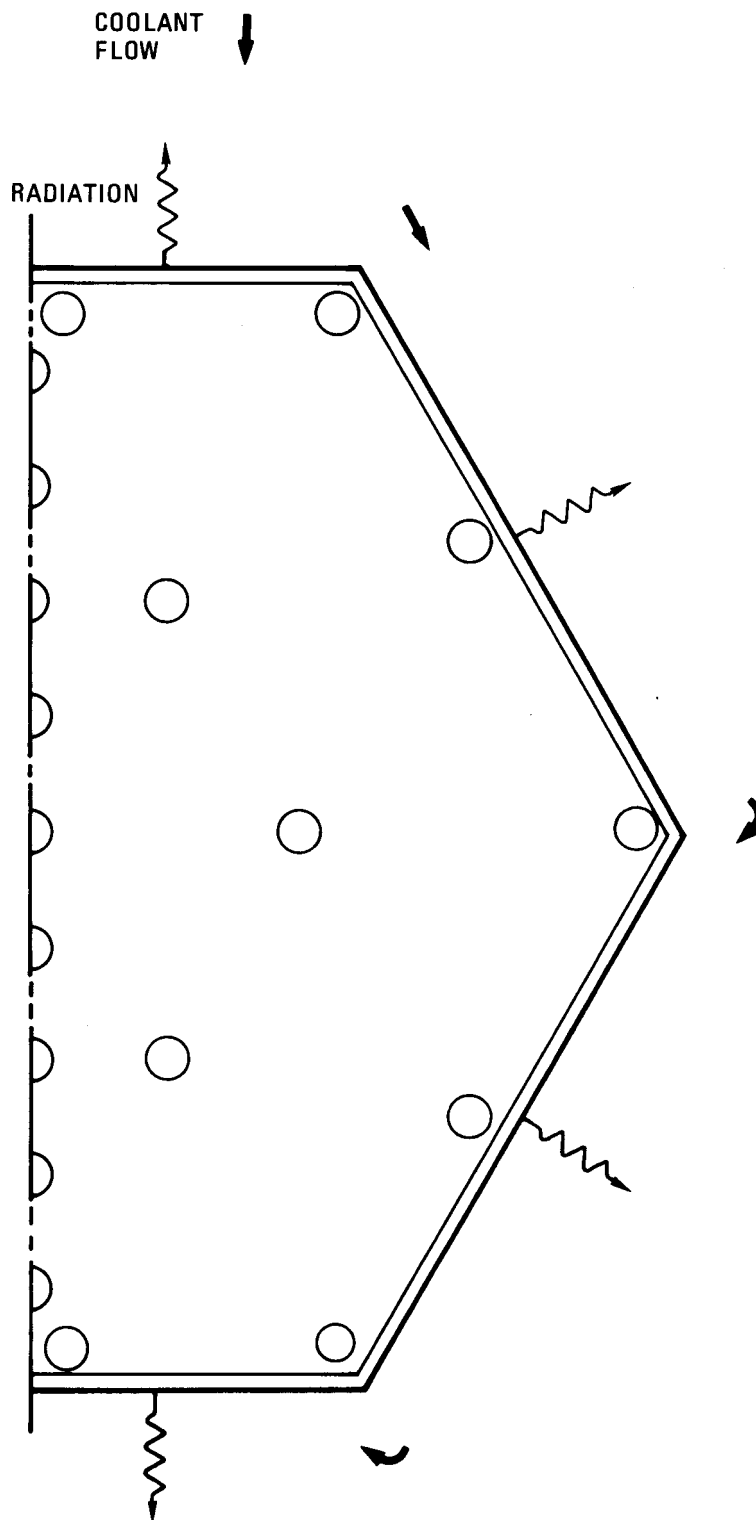


Fig. 2-3. Thermal model of assembly

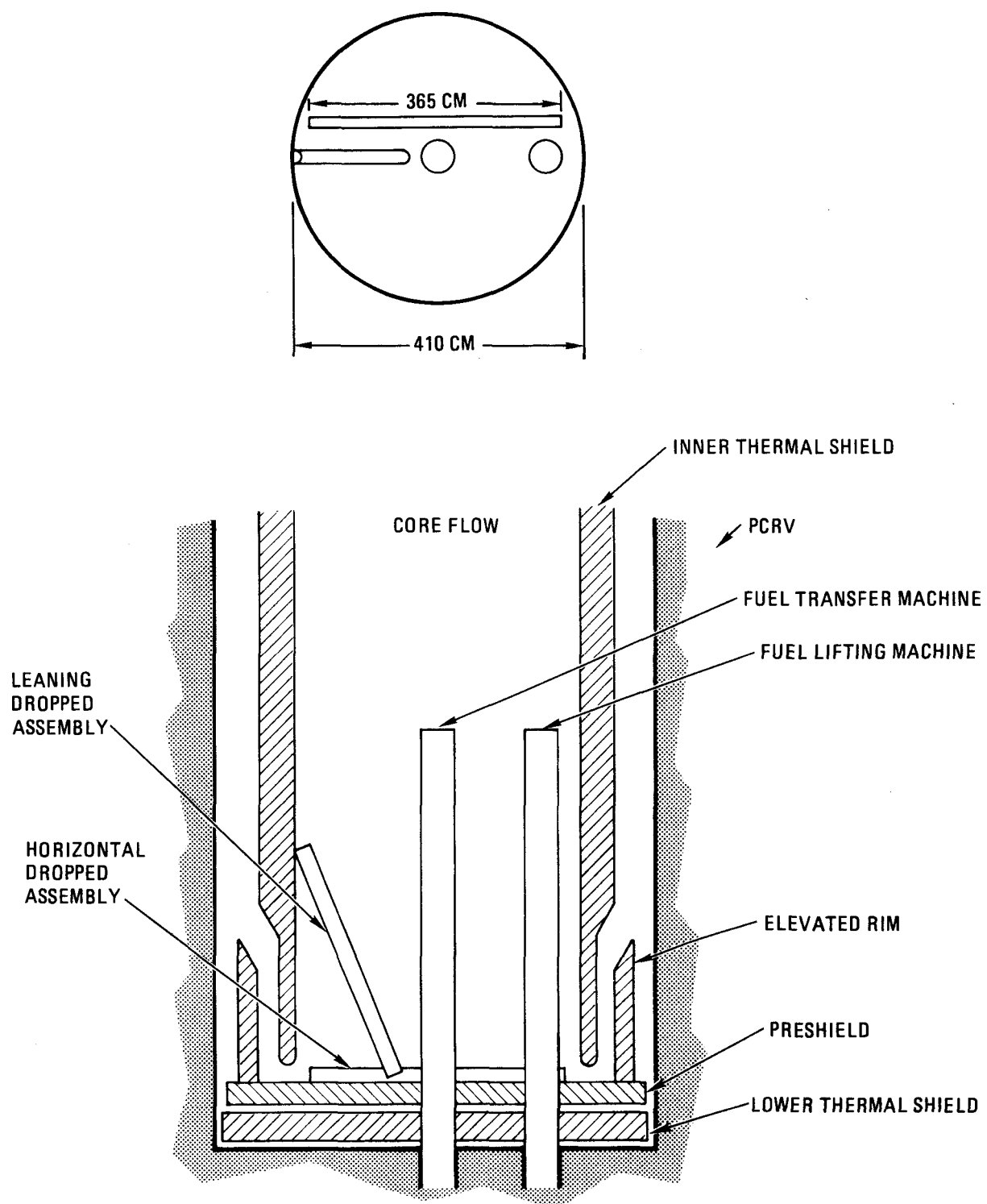


Fig. 2-4. Lower PCRV cavity

the assembly leaning against the shield, the coolant flow from the core sweeps around the dropped assembly as it moves toward the exits at the bottom of the cavity (Fig. 2-4). This configuration approximates that of a cylinder in cross flow; equations from Ref. 2-7 were used to estimate the heat transfer coefficient variation as the flow passes around the duct. For a total core coolant flow rate of 51,000 kg/hr, 2% of the full power value, the coefficient varied from a high of $94 \text{ W/m}^2\text{-}^\circ\text{C}$ on the front face to a low of $40 \text{ W/m}^2\text{-}^\circ\text{C}$ on the side where the flow separates from the surface.

The rate of radiant heat transfer is strongly dependent on the emissivity of the surfaces. The emissivity of stainless steel increases with temperature, approaching a value of unity near the melting point. Since the rate of heat transfer is proportional to the surface emissivity, the use of a typical room temperature value of 0.65 in this analysis is conservative.

To determine the power generated in the dropped assembly, an analysis was performed which predicted the afterheat for unreflected assemblies as a function of time after shutdown. It was assumed that fuel handling operations could begin as early as three days after shutdown. For an average assembly exposed to 876 full-power days of operation, the total gamma plus beta ray emission rate three days after shutdown is 25.8 kW, and the corresponding deposition rate in an unreflected assembly is estimated to be 21.0 kW. For the center 30.5 cm of the assembly, the integrated power level is 29.8% of the total, or 6250 W. Therefore, a deposition rate of 3125 W was used for the SCEPTIC model.

In summary, the conservatisms built into the model include analysis of average-powered rather than minimum-powered assembly, low emissivity, minimum power decay time, nominal core coolant flow, no convective heat transfer inside the assembly, and no axial conduction from the center of the bundle. For the horizontal configuration an adiabatic lower face and natural convection on the outside of the duct were assumed. The conservatisms built into the analysis will more than compensate for the fact that the analysis

does not calculate temperature gradients in the rods and no hot spot factors have been included in the determination of peak temperatures.

Because of the presence of the adiabatic surface on the bottom face, the peak temperatures for the horizontal assembly are located on the symmetry line and below the assembly centerline. For an average external heat transfer coefficient of $12 \text{ W/m}^2\text{-}^\circ\text{C}$, corresponding to natural convection, the temperature gradient along the symmetry line in the upper half of the assembly is ten times steeper than that in the lower half. The calculated peak temperature of 1294°C is considerably below the value for incipient cladding melting. For the case of low external forced cooling, the temperatures of the duct wall and the outer row of rods declined significantly, but the peak temperature dropped by only 23°C .

For the leaning assembly, maximum convective heat transfer occurs at the top and decreases to a minimum value on the lower side face, where the flow separates from the surface. The somewhat higher value on the lower face is the result of turbulent eddies sweeping across the surface. Providing additional surface for external heat transfer causes a further 66°C drop in the calculated peak temperature, a total margin of 180°C below the melting point. Based on this analysis, there are three proposals for minimizing the possibility of melting in a dropped fuel assembly:

1. Arrange the refueling sequence so that the lowest-powered assemblies are handled first and the highest-powered last.
2. Provide a means for a dropped assembly to lean against the thermal shield rather than fall flat on the preshield. This could be achieved by providing concentric ridges on the preshield to prevent lateral slippage of the lower end of the assembly.
3. When an assembly has been accidentally dropped during refueling operations, increase the core cooling flow rate to provide additional external convective cooling.

The following conclusions have been reached:

1. None of the dropped assembly configurations analyzed contained rods which exceeded the cladding incipient melting temperature.
2. The conservatisms present in the model provide a high level of confidence that actual temperatures in a dropped assembly will not exceed the values calculated in this analysis.
3. Provisions should be made to (a) handle the assemblies in an order corresponding with increasing afterheat power levels, (b) prevent the dropped assembly from falling in a horizontal position on the preshield, and (c) increase the core cooling flow rate in the event that an assembly is dropped.

2.1.3. Radial Blanket Assembly Analysis

2.1.3.1. Maximum-Powered Radial Blanket Assembly Thermal-Hydraulic Performance at End and Beginning of Cycle. The radial blanket of the GCFR demonstration plant consists of two rows of assemblies, 90 in all, surrounding 127 fuel and control assemblies. The design characteristics of the radial blanket assembly are listed in Table 2-4. The radial blanket assemblies experience large axial and radial power gradients, and a detailed four-year management scheme has been devised to periodically rotate first-row assemblies and shuffle them to the lower-powered second row. With the new core configuration, the maximum-powered assemblies in the radial blanket are located in the middle of the "flats" of the first row (Fig. 2-5). From symmetry, it is seen that there is a total of 12 maximum-powered assemblies on the same minimum radius from the center of the core. Owing to the location of the maximum-powered assemblies, a "point" rather than a "flat" is closer to the center of the core, which means that a corner rod rather than a wall rod will have the highest power (Fig. 2-5).

The required coolant flow rate in any assembly is that necessary to limit the cladding midwall hot spot temperature to 700°C. Because of the

TABLE 2-4
RADIAL BLANKET DESIGN CHARACTERISTICS

Assemblies

Number	90
Length (mm)	3665.0
Duct i.d. across flats (mm)	169.0
Duct wall thickness (mm)	3.8
Duct material	20% cold-worked 316 stainless steel

Rods

Number of active rods	60
Number of instrumentation rods	1
O.D. (mm)	19.65
Wall thickness (mm)	0.50
Pitch (mm)	21.10
Pitch/diameter ratio	1.07
Nominal edge spacing (%)	100
Overall length (mm)	2230.0
Active length (mm)	2030.0
Blanket material	UO ₂
Smear density (% of theoretical density)	90.0
Cladding material	20% cold-worked 316 stainless steel

Spacers

Type	Helical wire wrap
Number	61
Wire diameter (mm)	1.40
Wire pitch (mm)	300.0
Wire start angles (deg)	0, 120, 240
Wire material	20% cold-worked 316 stainless steel

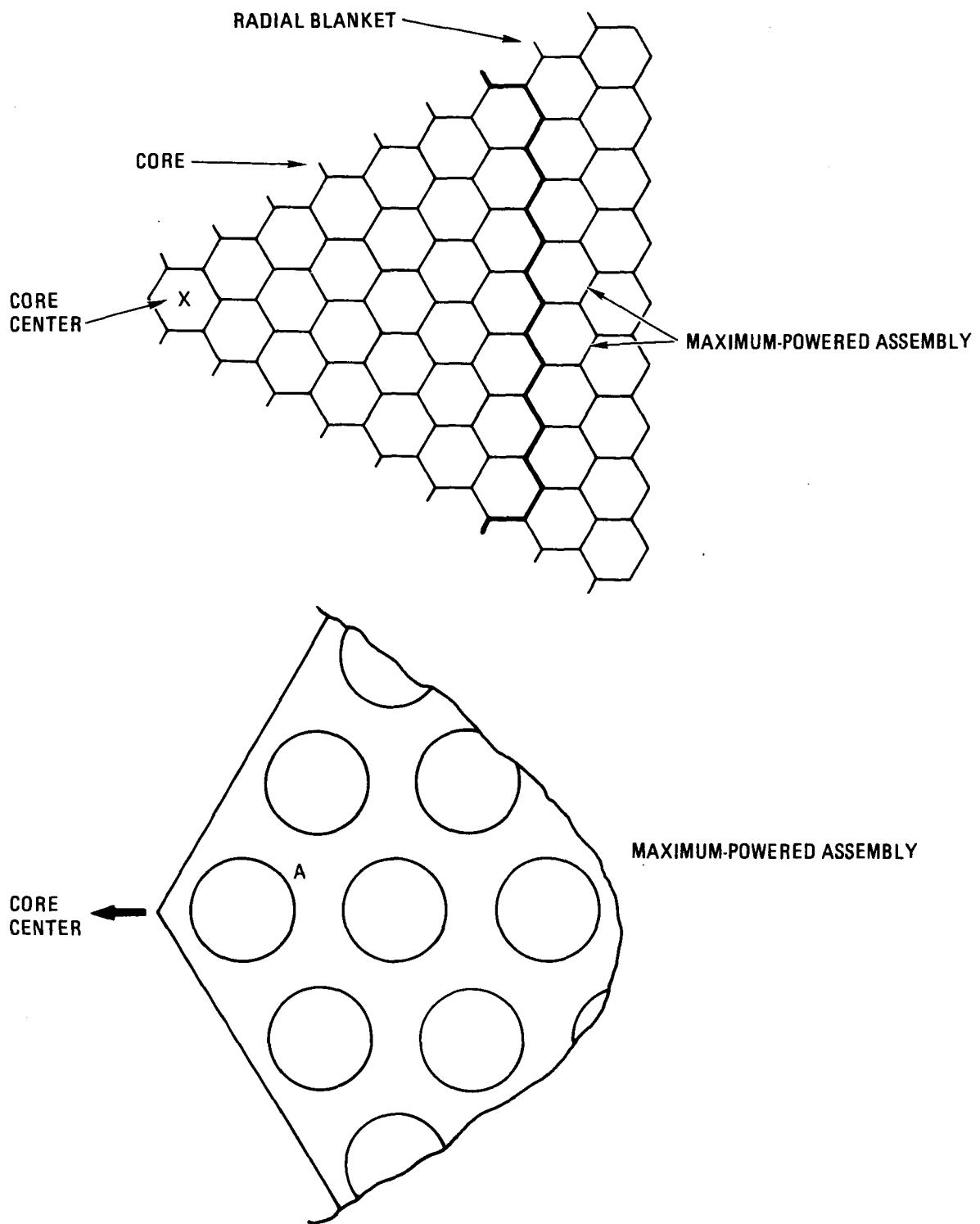


Fig. 2-5. Core configuration

large radial power gradient, the hot spot in a radial blanket assembly occurs on the rod nearest the core. To maintain the temperature limit, the rods farthest from the core are severely overcooled, resulting in a depressed assembly mixed mean outlet temperature. This is true even though the wire-wrapped spacer system promotes mixing within the assembly by coolant flow sweeping. In addition, the power in the radial blanket increases from the beginning to the end of a cycle, so that the assembly flow orifices must be sized for the coolant flows required at the end of cycle (EOC) rather than the beginning of cycle (BOC). This causes additional overcooling to occur at the beginning of a cycle.

A full 61-rod radial blanket maximum-powered assembly (Fig. 2-6) was modeled with the COBRA-IV subchannel analysis code (Ref. 2-8). Despite the presence of symmetry in the geometry of the assembly and in the power profiles, proper functioning of the wire-wrapped model requires that all the rods and wires be included. Thus, a total of 61 rods, 126 subchannels, and 186 subchannel connections had to be specified. In addition, the wire-wrapped model requires that the axial node length be no greater than one-sixth of the wire pitch, resulting in a total of 44 axial nodes.

The radial blanket was designed with a triple start-angle wire-wrapped configuration to increase the compliance, or flexibility, of the rod bundle. The COBRA-IV code does not correctly simulate the effects of this type of wire configuration, and tests are in progress to generate data to permit modification of the wire flow sweeping model. A single start-angle configuration is currently used in the radial blanket model.

The current normalized axial power profile for blanket rods is shown in Fig. 2-7. This profile is considered to be valid for both the end of equilibrium cycle (EOEC) and the beginning of equilibrium cycle (BOEC). The current normalized radial power profiles for the first row of blankets for EOEC and BOEC are shown in Fig. 2-8 (Ref. 2-9). Both distributions have much higher peak values than those of a typical fuel assembly. The

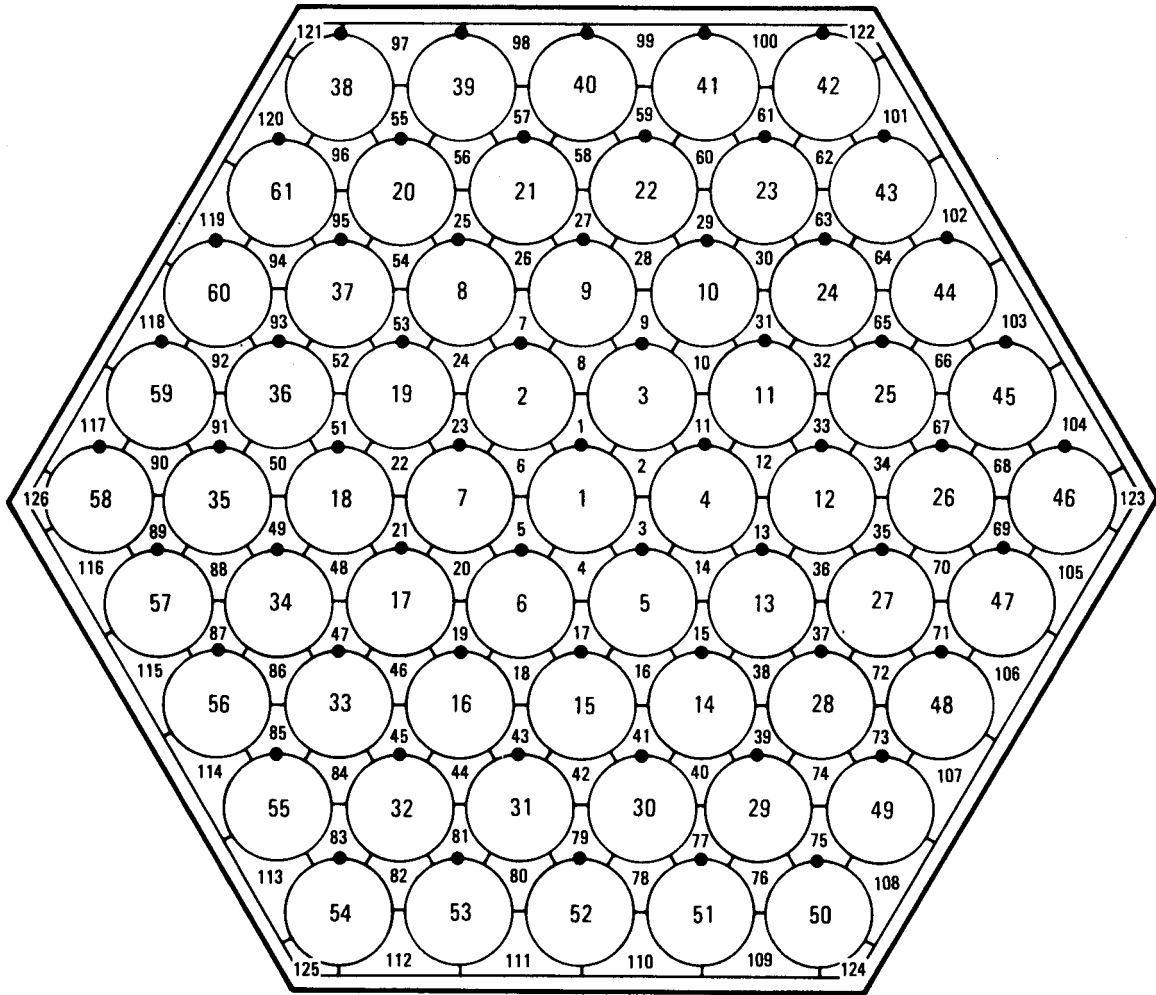


Fig. 2-6. Full radial blanket assembly model

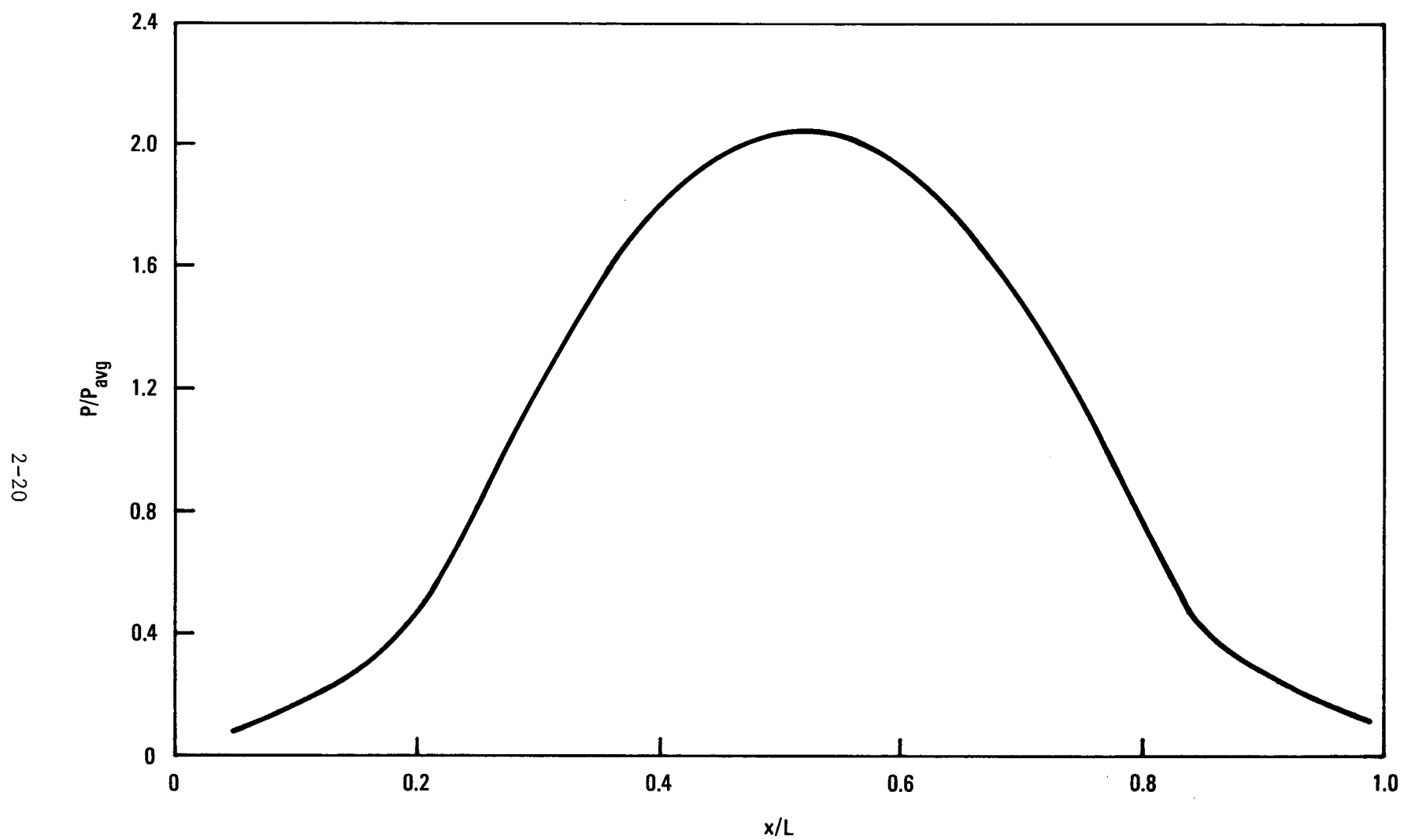


Fig. 2-7. Axial power distribution

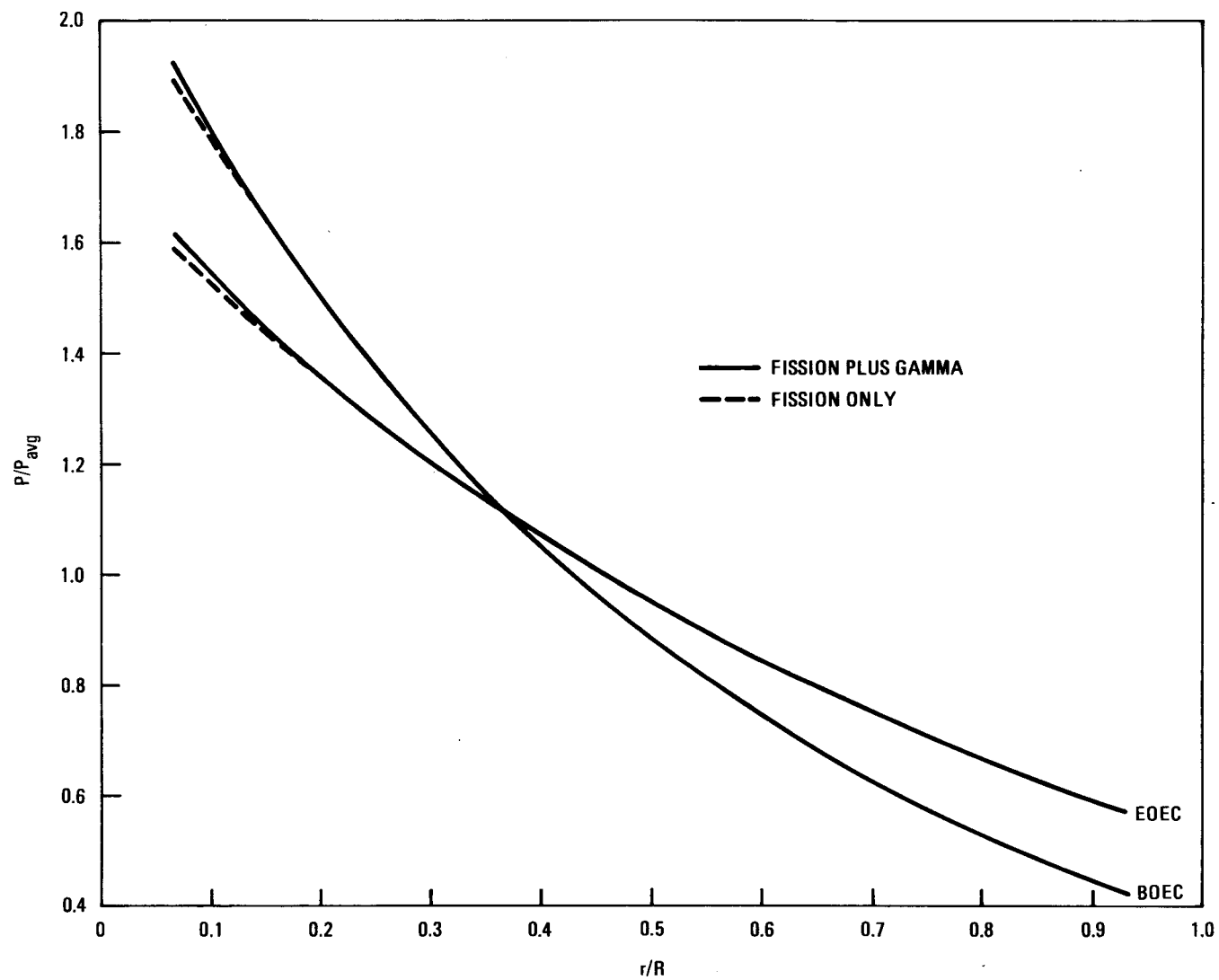


Fig. 2-8. Blanket radial power distributions at BOEC and EOEC

effects of gamma ray heating, which decay rapidly with radial distance from the core-blanket interface, have been included in the radial power distribution.

Hot spot factors for the radial blanket assembly have been developed and are reported in Ref. 2-10. A reevaluation of one of the component factors, that representing the circumferential variation of the heat transfer coefficient around a rod, has led to a modification of the Ref. 2-9 values (Ref. 2-11). The current values for the three factors are

Coolant	1.195
Film	1.775
Cladding	1.248

The analysis was first performed for the EOEC condition. The total coolant flow rate was parametrically varied to obtain a maximum cladding midwall hot spot temperature near 700°C. The resulting flow rate of 9460 kg/hr and other parameters are indicated in Table 2-5. Figure 2-9 shows the axial variation of the coolant and cladding midwall hot spot temperatures corresponding to the location in the bundle where the maximum hot spot temperature occurs (point A on Fig. 2-5) for EOEC and BOEC. The irregularity of the profiles is caused by the wire wrap alternately sweeping coolant into and out of the subchannel. The period of the cycles in the coolant profile corresponds to the wire wrap pitch; that of the cladding profile is one-third of the pitch. The cladding temperature fluctuations about a faired line are as large as $\pm 10^{\circ}\text{C}$. It should be noted, however, that the calculated cladding temperatures do not take circumferential conduction into account. Figure 2-10 shows the coolant temperature profiles along a radius from the center of the core at two axial locations. In general, the coolant temperatures are lower in the edge (and corner) subchannels and peak at approximately 25% of the point-to-point width of the assembly.

TABLE 2-5
RESULTS OF THERMAL-HYDRAULIC ANALYSIS OF
MAXIMUM-POWERED ASSEMBLY FOR EOEC

Calculated hot spot temperature	704°C
Mixed mean outlet temperature	466°C
Maximum ΔT across a rod	66°C
Overcooling ^(a)	64%
Bundle pressure drop	190 kPa
Total coolant flow rate	9460 kg/hr
Average exit velocity	78 m/s

^(a) Coolant temperature rise in maximum-powered/
average fuel assembly coolant.

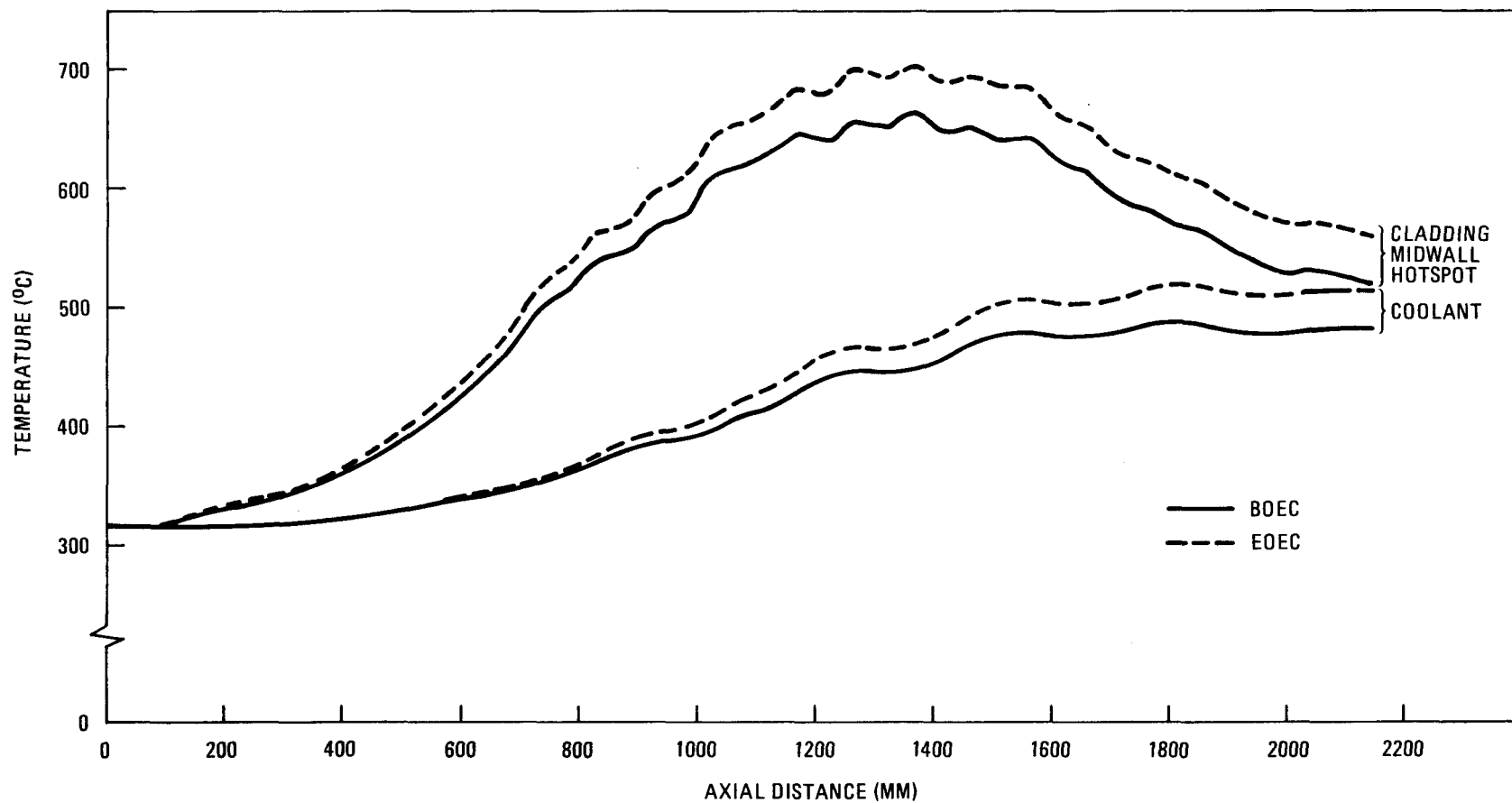


Fig. 2-9. Axial temperature distributions at hot spot location for EOEC and BOEC

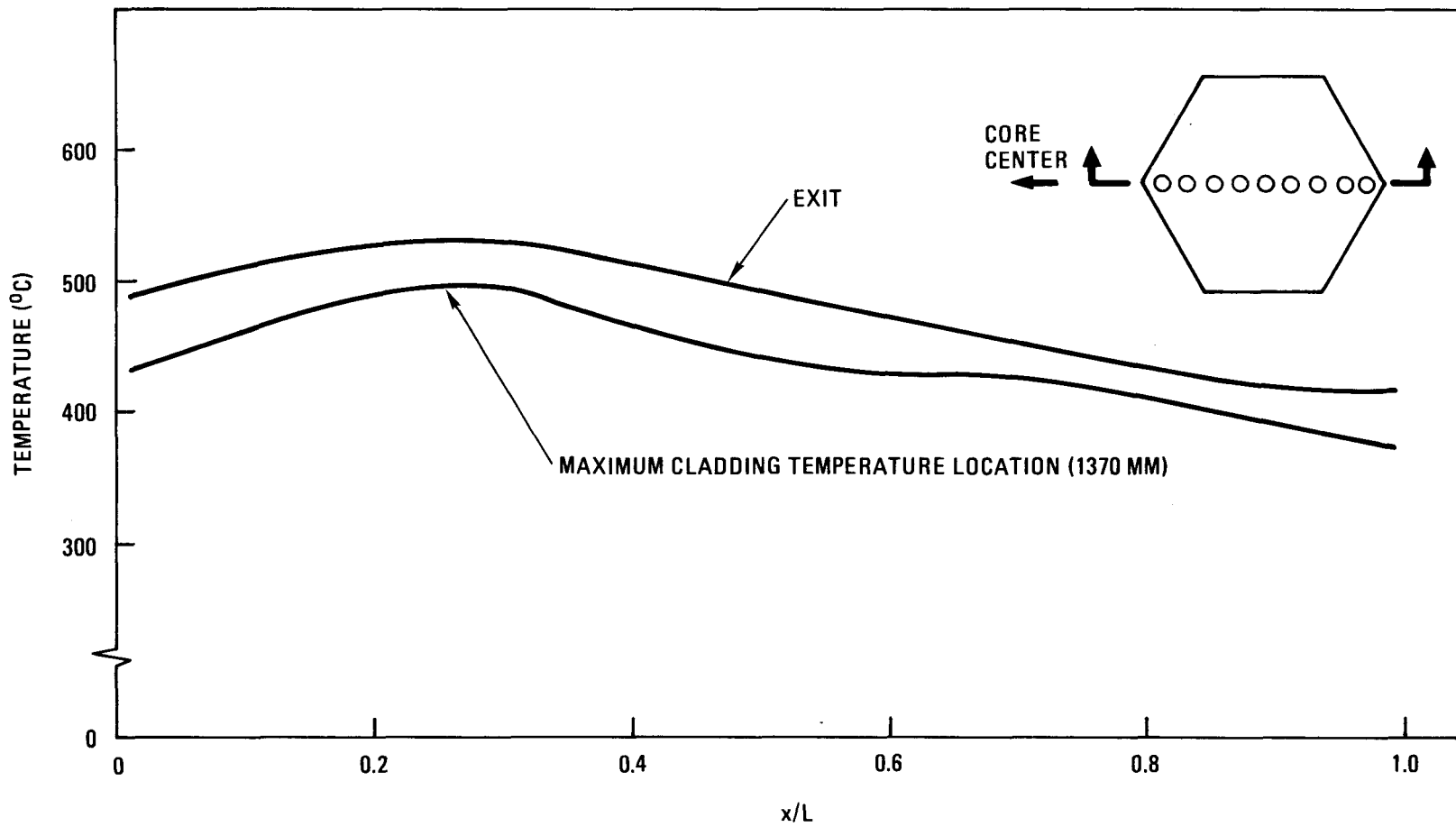


Fig. 2-10. Coolant radial temperature profile at two axial locations for EOEC

Next, the thermal-hydraulic analysis of the radial blanket maximum-powered assembly was performed for the BOEC condition. The COBRA-IV model used was the same as that used for EOEC, except for the differences in radial power profiles and power levels. Even though the radial power profile for BOEC is considerably steeper than that for EOEC (Fig. 2-8), the EOEC condition was anticipated to be more severe with regard to the maximum cladding hot spot temperature owing to a 30.4% higher assembly power for the EOEC condition. For this reason, the maximum-powered assembly orifice setting and the mass flow rate for the EOEC condition were assumed.

The results of the analysis confirm that the BOEC condition is less severe than the EOEC condition. The maximum cladding midwall axial temperature distribution for the maximum-powered rod and the adjacent subchannel temperature distribution are shown in Fig. 2-9 for the BOEC condition along with the results for the EOEC condition. For BOEC, the predicted maximum cladding midwall hot spot temperature was 663°C, and the maximum temperature difference across a rod, not allowing for circumferential cladding conduction, was 58°C. The rod bundle pressure drop was 182 kPa, and the mixed mean outlet temperature was 428°C. Therefore, BOEC represents a less severe but more overcooled condition than EOEC.

2.1.3.2. Simplified COBRA-IV Model for Radial Blanket Analysis. The COBRA-IV subchannel analysis code is fully capable of predicting local pressures, temperatures, and flow rates within the wire-wrapped radial blanket assembly. However, proper application of the wire-wrapped model requires that the full cross section of the bundle be modeled and the axial node length be no greater than one-sixth of the wire pitch. Consequently, the problem is very large, requiring rolling of the data between the computer core and peripheral storage units during execution and printout. Run times and costs are high with this technique, and therefore development of a much simpler model was desired. This model would be capable of calibration so that it could adequately simulate the most important results (peak cladding temperatures) of the full model and then be used for parametric analyses of the effects of changing operating conditions. Such a model has been developed and is described below.

The model for the full bundle is described in Section 2.1.4.1. The simplified model, shown in Fig. 2-11, simulates only one-third of the bundle cross section and requires only 43 subchannels and 25 rods. In addition, the wire-wrapped model has been eliminated, and to partially compensate for the removal of the wires, two modifications of the input data have been made. The input value for mass flux was reduced to maintain the same inlet mass flow rate with the increased flow area, and the turbulent mixing factor was accordingly increased to simulate the flow sweeping effect of the wire wrap. These features have resulted in large savings of computer run time and cost.

The turbulent mixing factor (β) was varied parametrically until good agreement with the results of the full radial blanket bundle analysis was obtained with regard to maximum cladding midwall hot spot temperature, maximum-powered rod axial temperature profiles, and adjacent subchannel axial temperature profiles. To assess the magnitude of the wire wrap effect, the value of β was initially the same as that in the full-bundle model ($\beta = 0.02$). This analysis was performed for the power levels and power profiles corresponding to EOEC, for which the results of the full-bundle analysis were already available. For this case, a maximum cladding midwall hot spot temperature of 730°C was predicted, which is 26°C higher than that predicted with the full-bundle model. This difference is due to the insufficient simulation of the flow sweeping effect of the wire wrap. The results of this case are presented in Fig. 2-12 along with results of the full-bundle analysis.

To increase the flow sweeping effect in the simplified model, β was parametrically increased. With $\beta = 0.08$, the maximum cladding hot spot temperature of 696°C was predicted, as shown in Fig. 2-12. This is in good agreement with the results of the full-bundle model when the cladding temperature fluctuations are faired. In general, good agreement was obtained for the entire cladding temperature axial distribution, except that the simplified model could not predict the temperature fluctuations due to the wire wrap alternately sweeping coolant into and out of a

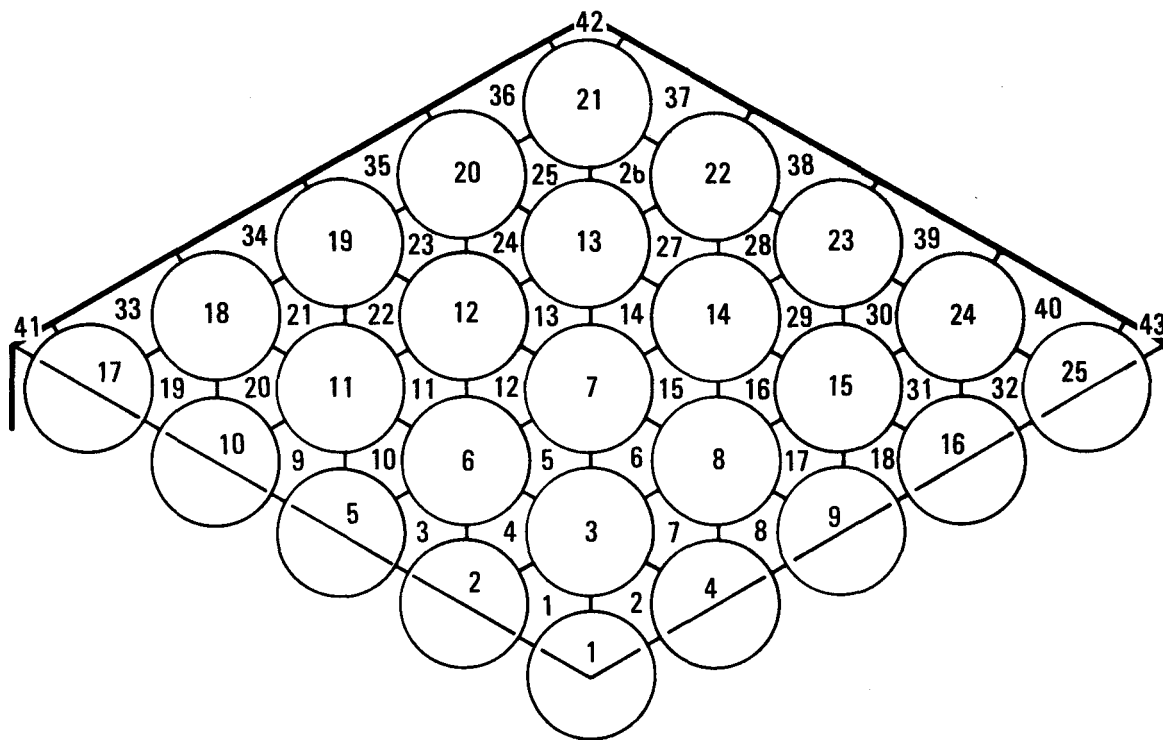


Fig. 2-11. Simplified computer model

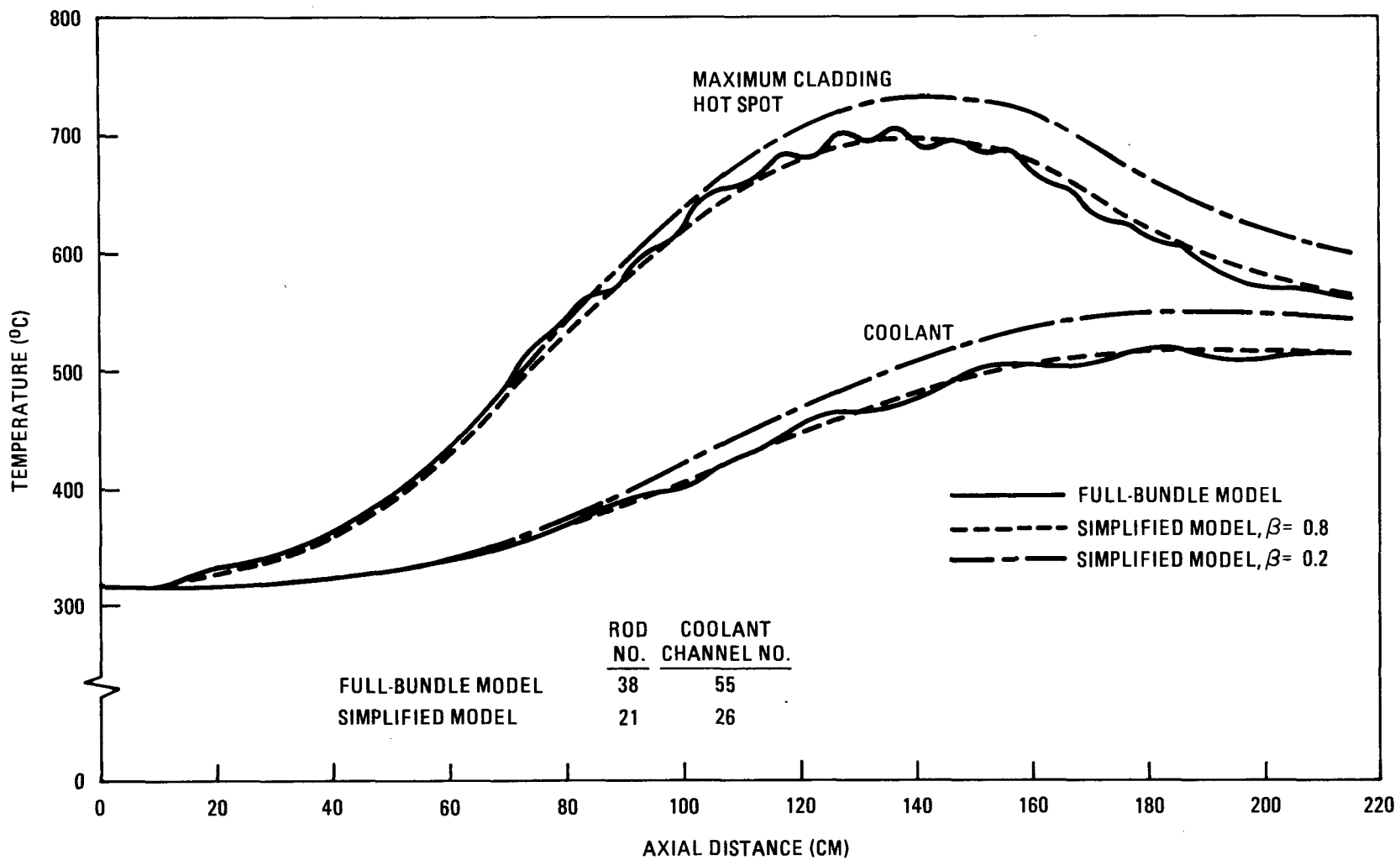


Fig. 2-12. Axial temperature distributions at EOE

subchannel. For $\beta = 0.08$, the simplified model slightly underpredicted the cladding midwall temperatures upstream of the hot spot and slightly overpredicted the temperatures downstream of the hot spot. At the exit, cladding midwall temperatures of 559° and 564°C were predicted for the full-bundle and simplified models, respectively. The cladding temperature distributions shown in Fig. 2-12 are for the maximum-powered blanket rods; rods 38 and 21 are the full-bundle and simplified models, respectively.

The coolant axial temperature distributions are also shown in Fig. 2-12 for the full-bundle (subchannel 55) and the simplified model (subchannel 26). Again, there was good agreement of the two models. As discussed previously, the simplified model cannot account for the temperature fluctuation due to the wire wrap, but the temperature profile predicted with the simplified model approximately coincides with the curve faired through the fluctuations. At the exit, subchannel temperatures of 514° and 515°C were predicted with the full-bundle and simplified ($\beta = 0.08$) models, respectively.

To verify the simplified model for a different condition, the analysis was performed for BOEC with $\beta = 0.08$. The assembly and radial power distributions for BOEC are different from those for EOEC. The maximum cladding midwall axial temperature distribution for the maximum-powered rod and the adjacent subchannel temperature distributions are shown in Fig. 2-13 for BOEC along with the results of the full-bundle analysis. Again, there was good agreement between the results of the full-bundle and the simplified models regarding the maximum cladding hot spot temperatures, subchannel temperatures, and general trends of the axial profiles.

The simplified model converges in fewer iterations, and the computer time per iteration is much lower than that for the full-bundle model. The main reason for this is that for the simplified model, the time-consuming rolling of the data between the computer core and the peripheral storage units does not have to be performed. This results in a decrease in the total iteration time by more than a factor of 20 with a corresponding savings in run cost.

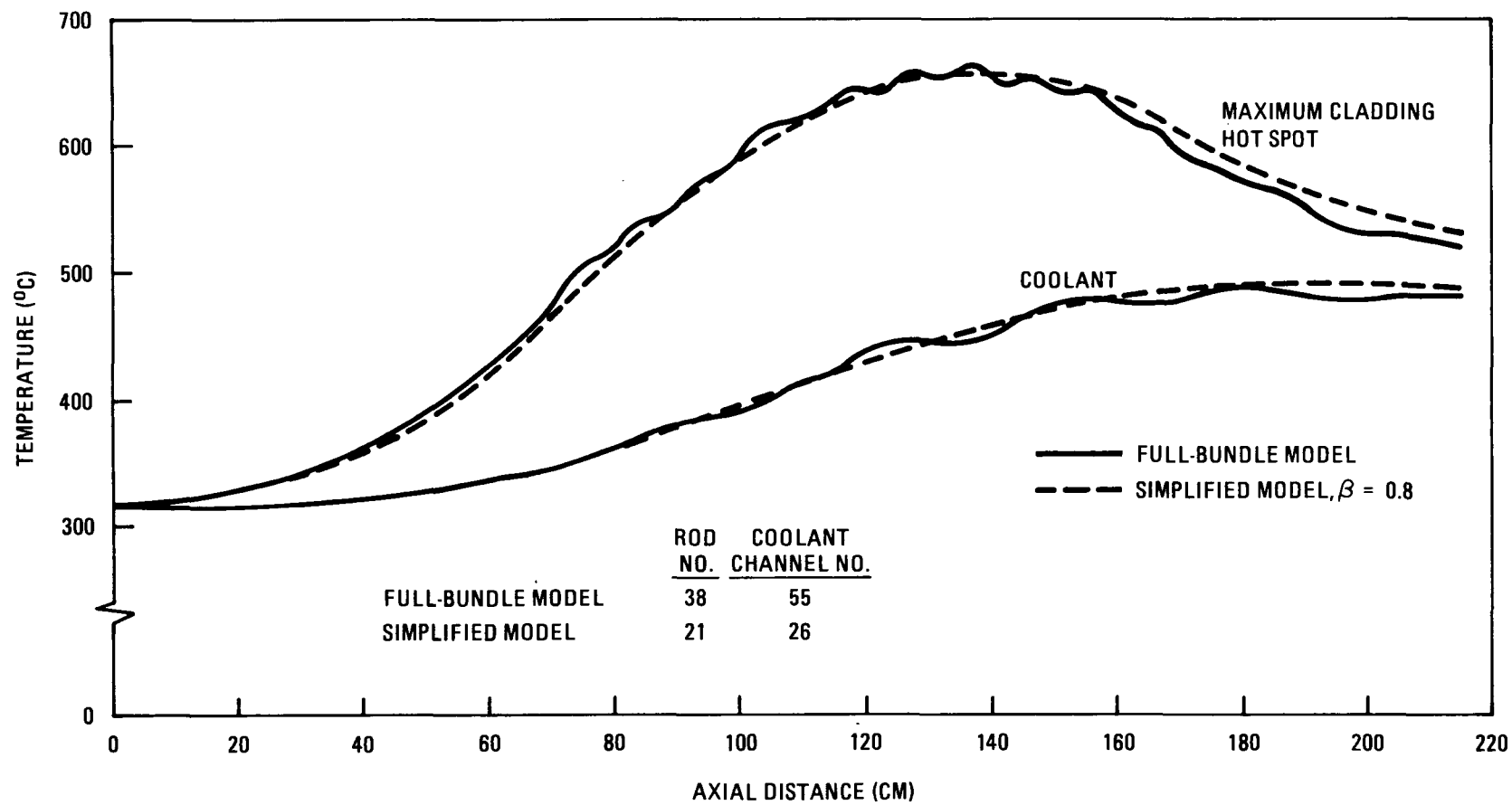


Fig. 2-13. Axial temperature distributions at BOEC

2.1.3.3. Adaptation of COBRA-IV to GCFR. Efforts to tailor COBRA-IV to the requirements of the GCFR are continuing. It was necessary to develop and incorporate property correlations for carbon dioxide into the code to analyze data from the AGATHE 1C tests performed at the Swiss Federal Institute for Reactor Research (EIR). A procedure similar to that used for helium properties was employed. No convergence problems have been encountered in the analysis of CO₂-cooled bundles.

To reduce the number of versions of the COBRA-IV code and to ensure that the original code can be traced to its originator [Battelle Northwest Laboratories (BNWL)], all the recent modifications of the code have been combined into one master version which will handle all coolants of interest (liquids, steam, helium, carbon dioxide) and both roughened and wire-wrapped rod bundles. The three sample problems supplied with the code have been rerun on the new master file, and the steady-state results have been satisfactorily reproduced. Sample roughened and wire-wrapped rod bundle calculations are being repeated to verify the reproducibility of the helium data.

2.2. CORE ASSEMBLY MECHANICAL ANALYSIS

An updated evaluation of the nominal bowing of GCFR edge rods (rods located at the periphery of the rod bundles adjacent to the flow duct wall) has been completed. The studies considered bowing of edge rods caused by thermal loading, irradiation swelling, and creep, but did not consider the effects of tolerances or flow duct dilation (hence the term nominal bowing). Results have been obtained for the current reference design spacer grid layout as well as several alternate layouts. The current reference 10-spacer layout does not allow the fuel assembly to reach its design goal burnup of 8.64 TJ/kg. A rearranged 10-spacer layout which has potentially acceptable nominal bowing has been developed. Detailed evaluation of the local hot spots caused by bowing and flow-induced fuel rod vibrations have not yet been performed for the revised configuration.

Several important conclusions can be drawn from this analysis:

1. Edge rod bowing may be a lifetime limiting factor for both hexagonal and round GCFR fuel assemblies.
2. In the current fuel assembly design, severe fuel rod bowing is predicted to occur just above the end of the active core. The current analysis indicates that rod-to-duct contact (in the active core region) would occur at about 6.9 TJ/kg ($\sim 85\%$ of design life).
3. No duct contact is predicted with an optimized spacer grid arrangement, although the nominal bowing at design end of life (EOL) will still consume over 63% of the edge rod-to-duct gap.
4. The current approach taken in the thermal-hydraulic optimization of the edge channel spacing (i.e., equalizing the absolute values of the fuel rod lateral temperature gradients in the smooth and rough regions of the core) is probably not the optimum approach, since both the amount and the consequences of bowing increase toward the end of the active core.
5. Current bowing limits are extremely restrictive and should be reevaluated.
6. There is a strong case for increased spacing between the edge rods and the duct if the same, or nearly the same, transverse rod temperature gradients can be maintained. Increases to about 75% and 100% of the rod-to-rod gap are projected to be required for the circular and hexagonal assemblies, respectively.

2.2.1. Bowing Analysis Description

The purpose of this analysis was to evaluate the nominal bowing of edge rods in the fuel assemblies of the 300-MW(e) demonstration plant GCFR.

The term nominal indicates that (1) nominal creep and swelling equations were used, (2) the effects of tolerances and duct dilation were neglected, and (3) possible thermomechanical feedback between the rod bow and the rod temperature distribution was ignored. Reference parameters for a 290-kPa ΔP fuel assembly for a 127-assembly core were used (Ref. 2-12), and corresponding thermal-hydraulic (Ref. 2-4) and physics (Ref. 2-13) data were employed. The dimensional parameters which were used are summarized in Table 2-6, and the thermal-hydraulic and physics data are presented in Table 2-7 and Fig. 2-14. (Midwall-to-midwall cladding temperature gradients were determined from the data in Ref. 2-12 using an area weighing procedure.) Material property data were taken from Ref. 2-14; the correlations for the 20% cold-worked 316 stainless steel fuel rod cladding creep and metal swelling were from the revisions 3 and 5, respectively, of Ref. 2-14.

The CRASIB code (Ref. 2-15) was used for this study. The CRASIB model of the edge rod consisted of 46 node points and 45 equal-length beam elements. A schematic layout of the fuel rod together with the model representation of the rod and the input physics and thermal data is shown in Fig. 2-15. The locations of the spacer grids are indicated by small triangles and are represented in the CRASIB model as simple supports. The reference design spacer layout as represented in the model is designated as spacer configuration 10-1. Two alternate 10-spacer layouts and one 11-spacer layout were also investigated. Configuration 10-2 was a 10-spacer layout with a 200-mm pitch through the active core region. Configuration 10-3 was an optimized 10-spacer layout with a 150-mm pitch in the lower part of the active core (where bowing is most severe) and a wider pitch between spacers elsewhere. The 11-spacer layout (configuration 11-1) had a uniform pitch of 150 mm between spacers in the active core region. It should be noted that the same thermal data were used for evaluation of the bowing of each of the various spacer layouts. Implicit in this is the assumption that the varying spacer locations would not affect rod temperature distributions enough to be significant from a bowing standpoint.

TABLE 2-6
 DIMENSIONAL DATA FOR EDGE ROD BOWING STUDIES
 (NOMINAL BOL COLD VALUES)

Cladding o.d. (root)	7.2 mm
Cladding i.d.	6.44 mm
Rod length	2230 mm
Rod-to-duct gap	1.4 mm
Rod-to-rod gap	2.8 mm

TABLE 2-7
INPUT FOR ROD BOWING STUDY: GCFR EDGE ROD, NOMINAL CASE

Node	Axial Location (mm)	Average Temperature (°C)	ΔT (°C)	Temperature Gradient (°C/mm)	Fast Flux (2.31 x 10 ¹⁵ n/cm ² -s)
1	0.00000	3.16000+02	0.00000	0.00000	1.00000-02
2	5.00000+01 (a)	3.16000+02	0.00000	0.00000	1.39970-02
3	1.00000+02	3.16000+02	0.00000	0.00000	3.06470-02
4	1.50000+02	3.16000+02	0.00000	0.00000	5.21840-02
5	2.00000+02	3.16160+02	0.00000	0.00000	9.59840-02
6	2.50000+02	3.16691+02	0.00000	0.00000	1.28496-01
7	3.00000+02	3.17685+02	0.00000	0.00000	1.59300-01
8	3.50000+02	3.19315+02	0.00000	0.00000	1.96152-01
9	4.00000+02	3.21234+02	3.08511-01	7.09873-02	2.40152-01
10	4.50000+02	3.23362+02	8.40426-01	1.93379-01	2.92400-01
11	5.00000+02	3.25862+02	1.00000+00	2.30097-01	3.54064-01
12	5.50000+02	3.28521+02	1.00000+00	2.30097-01	4.26276-01
13	6.00000+02	3.63692+02	6.09121+00	1.40157+00	5.10000-01
14	6.50000+02	4.06000+02	1.23000+01	2.83019+00	6.05584-01
15	7.00000+02	4.45362+02	1.53319+01	3.52782+00	7.02347-01
16	7.50000+02	4.82739+02	1.79804+01	4.13724+00	7.93377-01
17	8.00000+02	5.05565+02	1.78174+01	4.09972+00	8.77194-01
18	8.50000+02	5.13362+02	1.88319+01	4.33316+00	9.53287-01
19	9.00000+02	4.82511+02	2.28745+01	5.26334+00	1.02100+00
20	9.50000+02	4.69934+02	2.25308+01	5.18425+00	1.07800+00
21	1.00000+03	4.84769+02	1.56077+01	3.59128+00	1.12672+00
22	1.05000+03	4.99638+02	1.13426+01	2.60988+00	1.16436+00
23	1.10000+03	5.14532+02	9.00213+00	2.07136+00	1.19185.00
24	1.15000+03	5.28681+02	6.99957+00	1.60137+00	1.20912+00
25	1.20000+03	5.42511+02	5.04468+00	1.16076+00	1.21896+00
26	1.25000+03	5.54912+02	1.94506-01	4.47551-02	1.21162+00
27	1.30000+03	5.67000 02	-5.30000+02	-1.21951+00	1.19670+00
28	1.35000+03	5.77638+02	-6.36383+00	-1.46430+00	1.17151+00
29	1.40000+03	5.88043+02	-7.60652+00	-1.75024+00	1.13617+00
30	1.45000+03	5.96739+02	-1.01609+01	-2.33798+00	1.09000+00
31	1.50000+03	6.04638+02	-1.31468+01	-3.02504+00	1.03479+00
32	1.55000+03	6.10489+02	-1.72426+01	-3.96745+00	9.69902-01
33	1.60000+03	6.15758+02	-1.96121+01	-4.51268+00	8.96126-01
34	1.65000+03	6.20054+02	-1.93923+01	-4.46210+00	8.14453-01
35	1.70000+03	6.17372+02	-2.17681+01	-5.00876+00	7.25300-01
36	1.75000+03	6.09394+02	-2.80234+01	-5.98790+00	6.29437-01
37	1.80000+03	5.99553+02	-2.75606+01	-6.34161+00	5.31000-01
38	1.85000+03	5.88915+02	-2.79330+01	-6.42728+00	4.44444-01
39	1.90000+03	5.85663+02	-2.71087+01	-6.23762+00	3.69616-01
40	1.95000+03	5.84033+02	-2.60217+01	-5.98751+00	3.05600-01
41	2.00000+03	5.81915+02	-2.48011+01	-5.70664+00	2.51288-01
42	2.05000+03	5.79945+02	-2.36066+01	-5.43180+00	2.05488-01
43	2.10000+03	5.79396+02	-2.26725+01	-5.21687+00	1.67100-01
44	2.15000+03	5.78877+02	-2.18772+01	-5.03387+00	1.35024-01
45	2.20000+03	5.78439+02	-2.14386+01	-4.93295+00	1.06496-01
46	2.25000+03	5.78000+02	-2.10000+01	-4.83203+00	6.26960-02

(a) 5.00000+01 = 5.00000 x 10¹.

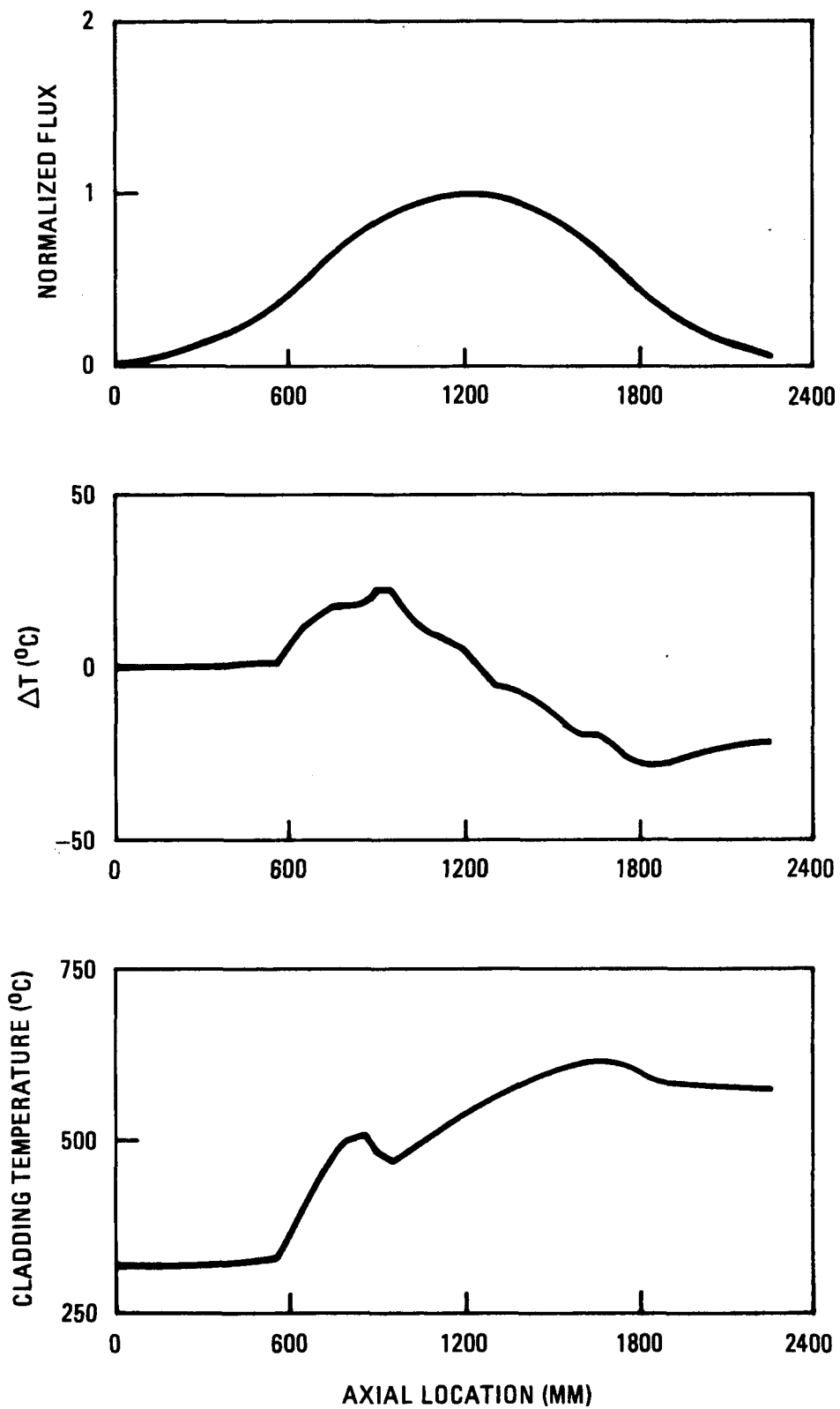


Fig. 2-14. Input for bowing study: GCFR edge rod, nominal case

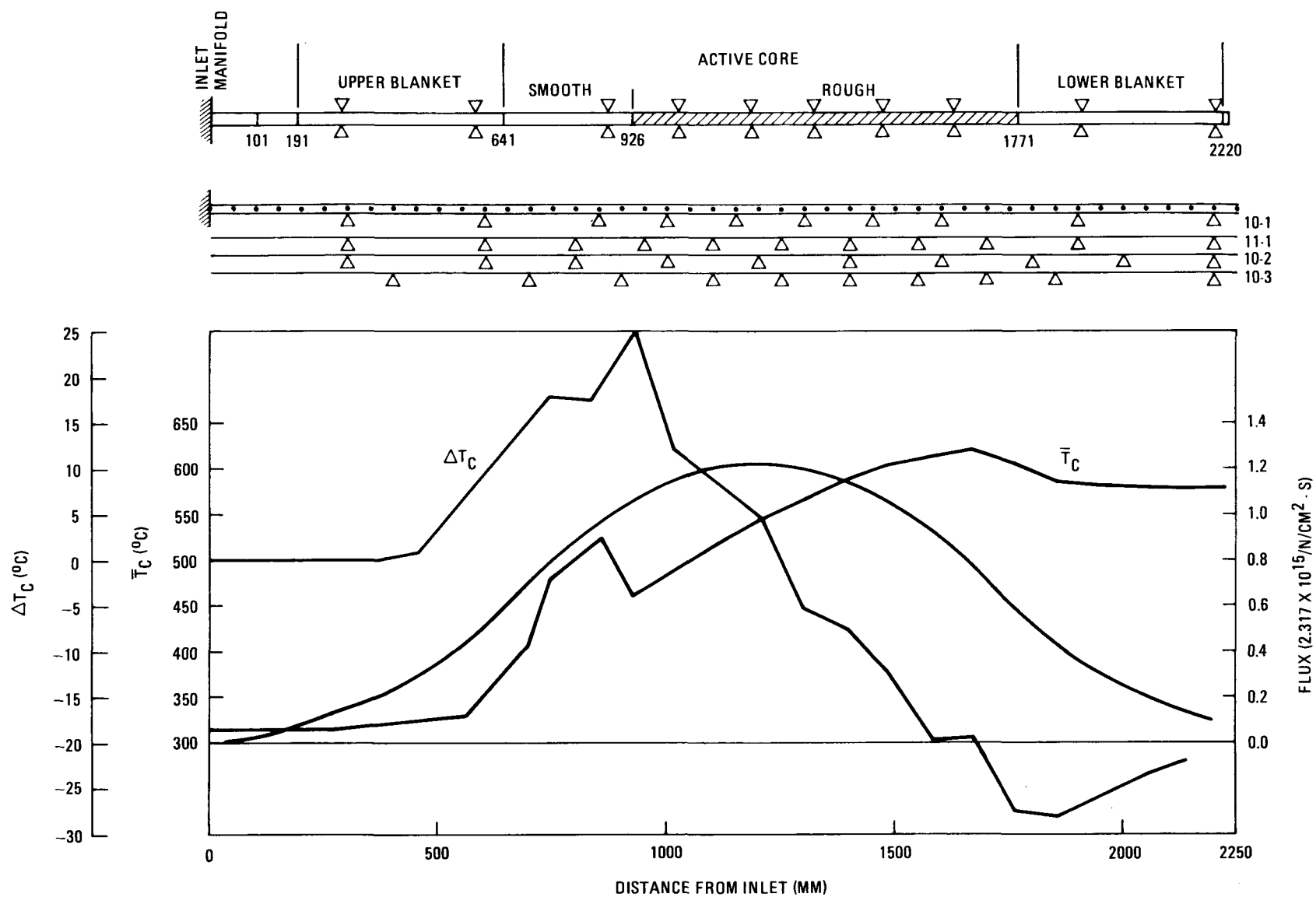


Fig. 2-15. Schematic of fuel rod and CRASIB model

2.2.2. Edge Rod Bowing Results

The nominal bowing at design EOL for the reference design and the three alternate spacer layouts is shown in Figs. 2-16 through 2-19. There is little difference between the in-core bowing results for the 10-spacer configuration 10-3 and the 11-spacer configuration 11-1. In addition, spacer configuration 10-2 results in considerably greater bowing than configuration 10-3. Hence, further discussion is limited to the reference design spacer layout, configuration 10-1, and the optimized 10-spacer layout, configuration 10-3.

For the purpose of comparison, Fig. 2-20 shows the bowing of the two spacer configurations. The bowing results for the optimized configuration are plotted for a design EOL of 21,000 hr, and bowing of the as-designed layout is shown for 18,000 hr. The nominal position of the (undilated) fuel assembly duct wall is plotted at the top of the graph. As can be seen, rod-to-duct contact (in the active core region) is predicted to occur for the reference design by 18,000 hr. (It should be noted that although duct dilation may be sufficient to prevent rod-to-duct contact for edge rods near the center of a hexagonal duct flat, it cannot be counted upon to prevent contact of rods closer to the corners of the hex, nor will it have any significant effect in circular assemblies.) No contact is predicted for the optimized spacer configuration. However, the positive rod bow near the center of the core does consume over 63% of the edge rod gap, and the negative rod bow consumes about 33% of the space available for bowing toward the next inner row of rods.

The present analysis indicates that rod bowing is deflection limited rather than stress limited. Just how much deflection is acceptable has not been definitely established, although a tentative criterion based on LMFBR experience has been proposed (Ref. 2-16). This criterion states that "the area change due to rod bow of the subchannel adjacent to an edge rod shall be less than 10%." To illustrate the effects of various rod bowing criteria on assembly lifetimes, the peak positive and negative bows for the two

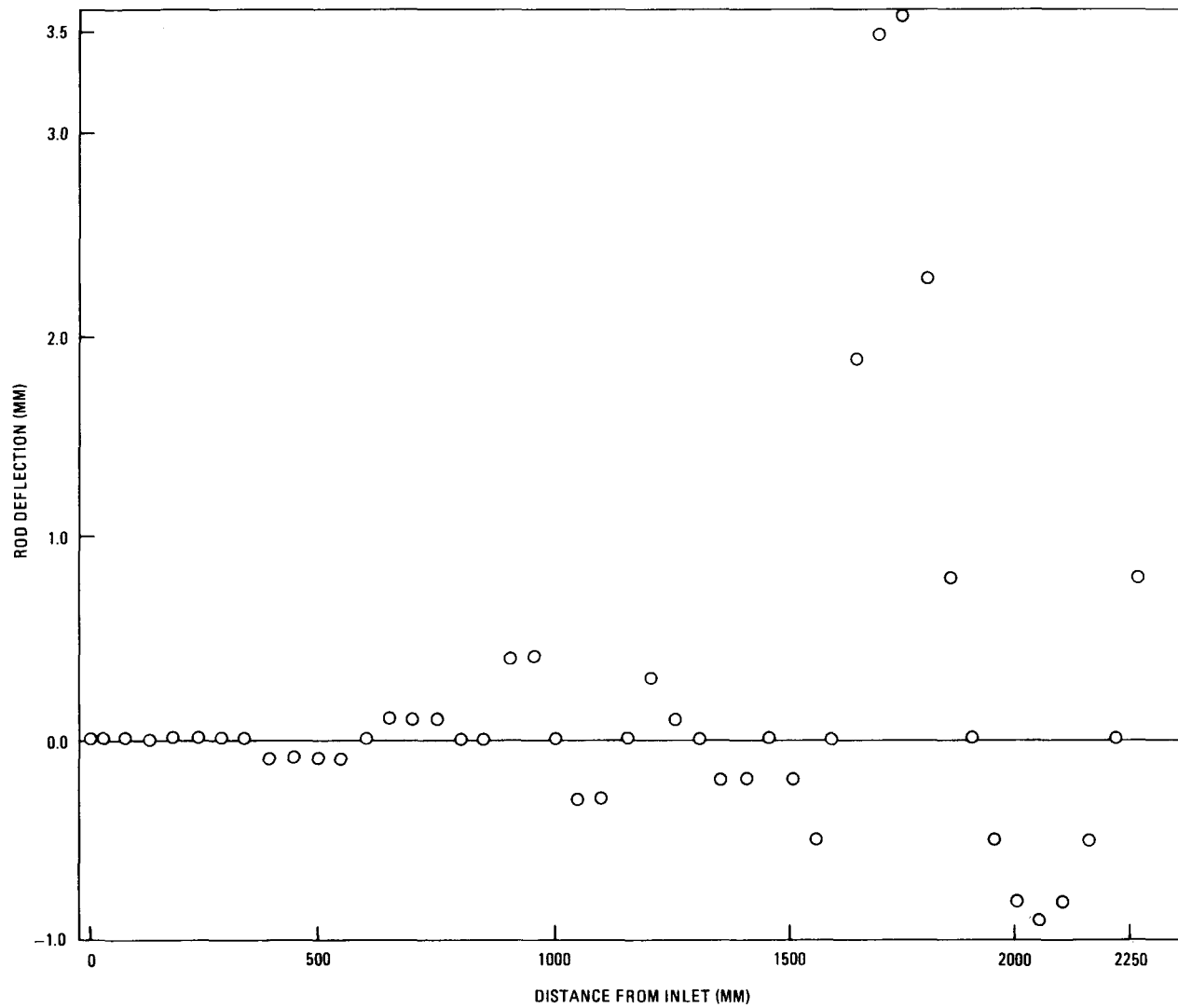


Fig. 2-16. Rod bowing at EOC (configuration 10-1)

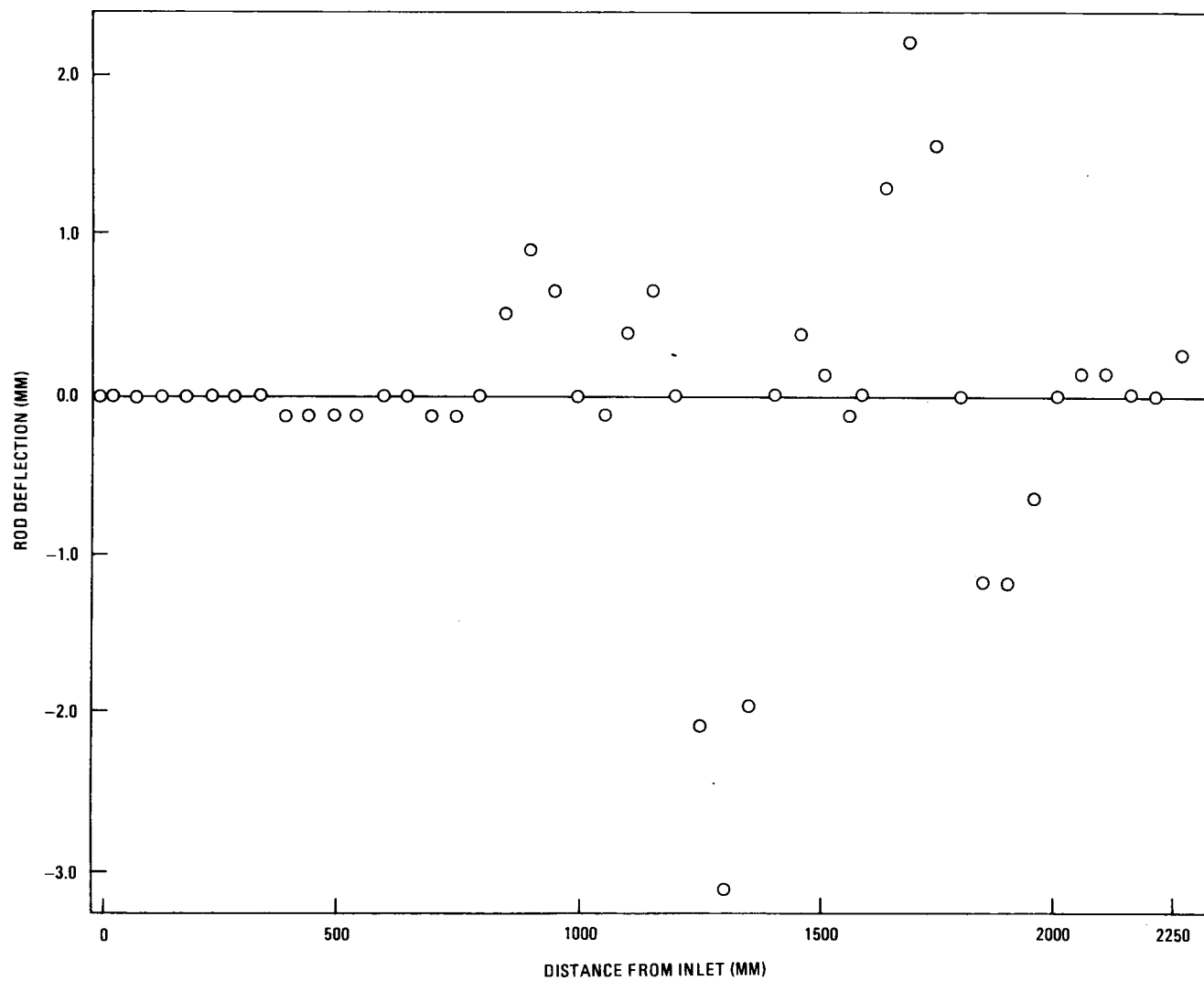


Fig. 2-17. Rod bowing at EOC (configuration 10-2)

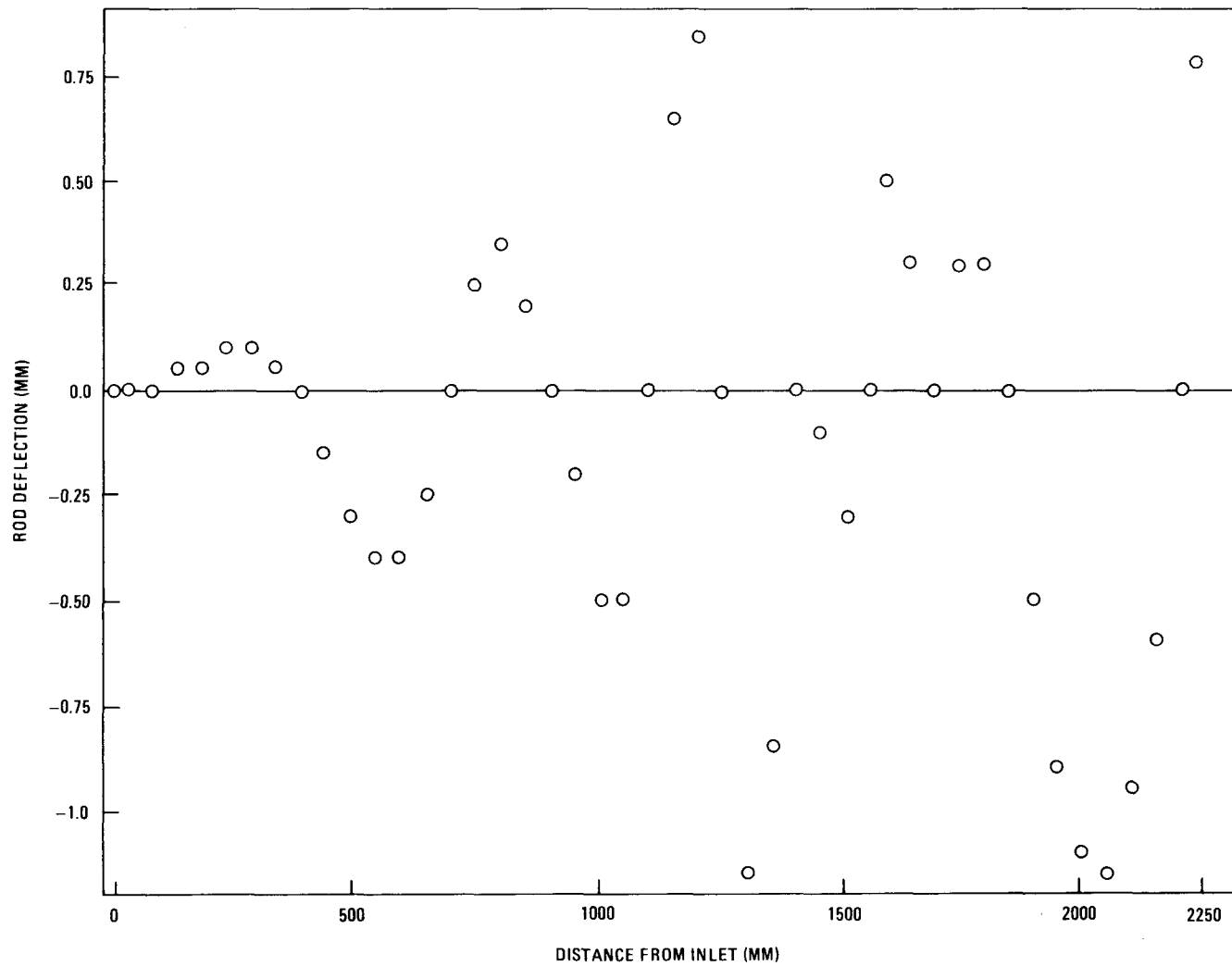


Fig. 2-18. Rod bowing at EOC (configuration 10-3)

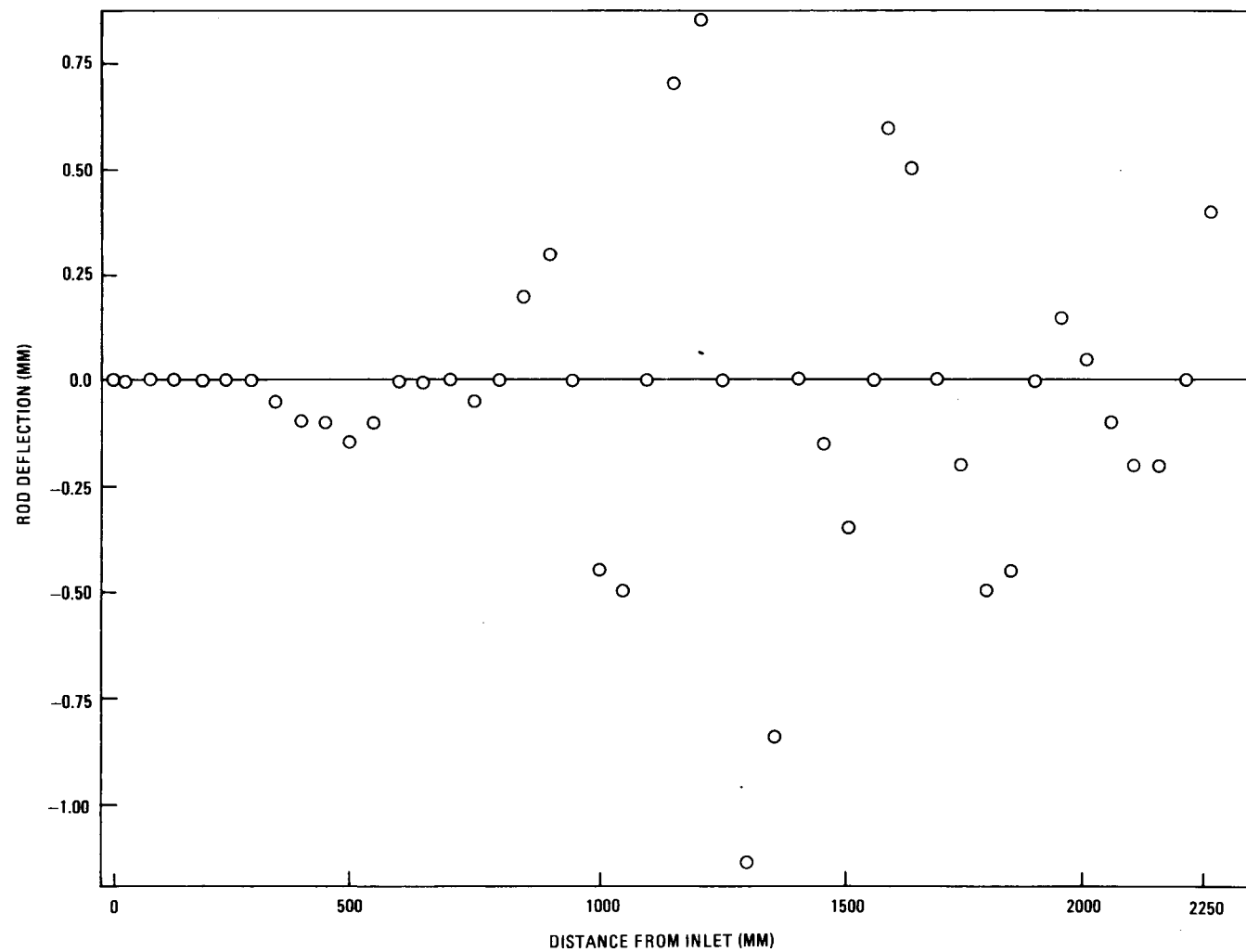


Fig. 2-19. Rod bowing at EOC (configuration 11-1)

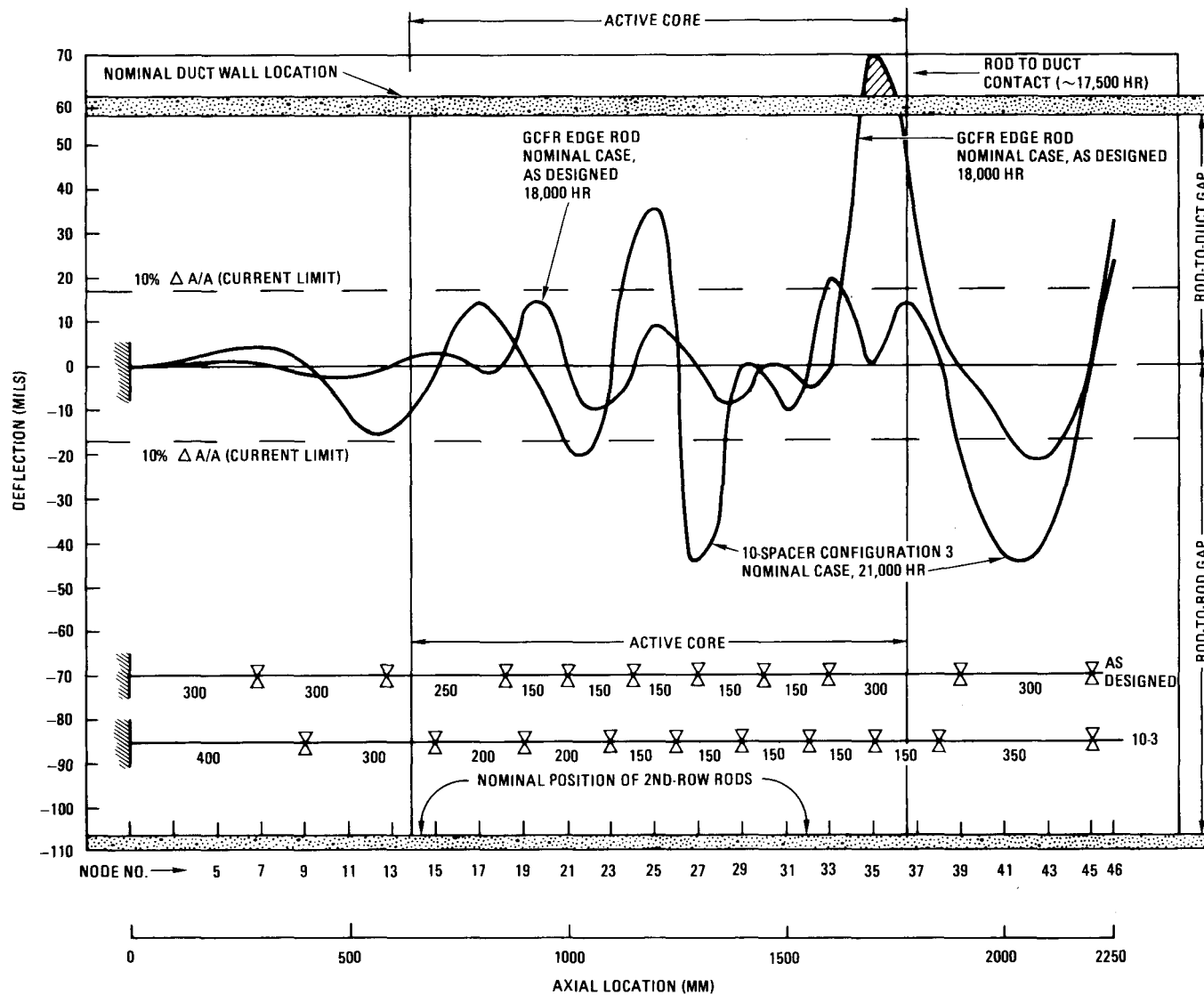


Fig. 2-20. Comparison of EOL bowing of as-designed and optimized spacer layouts

spacer configurations are plotted as functions of the time in Fig. 2-21. The 10% $\Delta A/A$ criterion is shown as a dashed line and is very restrictive. If this criterion were used, the fuel assembly lifetime would be restricted to about 50% and 60% of the reference and the optimized design, respectively. The maximum bowing limit which could be conceived was rod-to-duct contact. This would not be a lifetime limiting factor for the optimized design, but would restrict the reference design to less than 85% of its design life.

As mentioned above, rod bowing appears to be deflection limited rather than stress or load limited. In general, stresses are below ~ 0.1 GPa, spacer reactions are on the order of about 5N, and total axial elongation of the rod is more than 85 mm. Peak stress, strain, load, and deflection data for the two designs are presented in Table 2-8.

2.2.3. Conclusions

Nominal bowing of edge rods in the reference design fuel assembly was evaluated for the reference spacer grid and several alternative layouts. The analysis employed nominal creep and swelling correlations and used beginning of life (BOL) temperature distributions which did not consider the effects of duct dilation, tolerances, or possible thermomechanical feedback from rod bowing. These are nonconservative simplifications which would tend to cause the analysis to underpredict the actual values. On the conservative side, circumferential conduction and power reduction due to fuel burnup, which would tend to cause the analysis to overpredict the actual values, were also neglected. The current predictions should be most applicable to the bowing expected of edge rods located away from the center of the flats of hexagonal duct assemblies, and provided that the same temperature distribution applies, the current predictions should also apply reasonably well to circular duct fuel assemblies. The major uncertainty in the analysis is probably associated with the uncertainty in the amount of irradiation swelling which will occur in the later part of life. Unfortunately, this uncertainty will probably not be significantly reduced for several years.

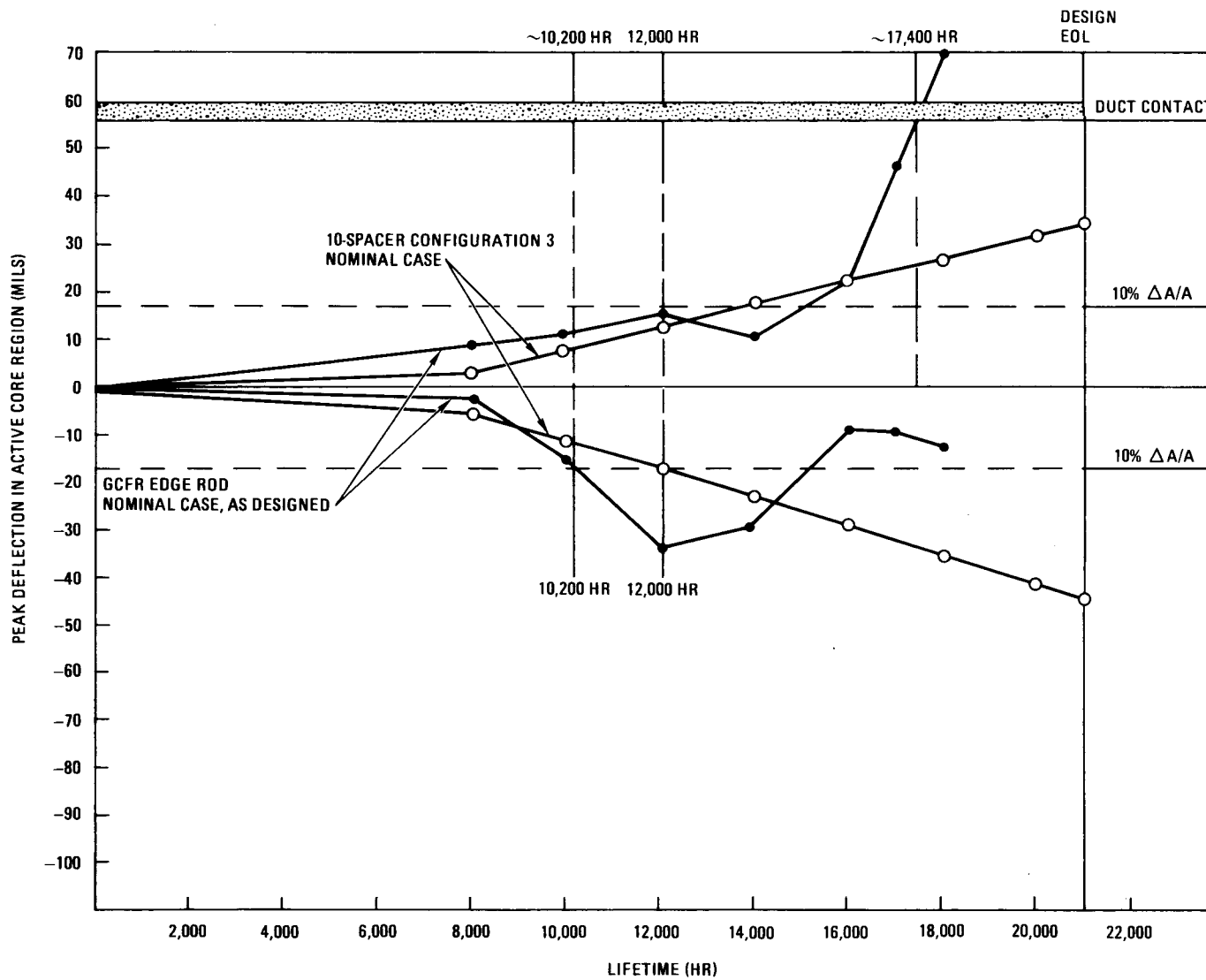


Fig. 2-21. Effect of different bowing criteria on assembly lifetimes

TABLE 2-8
SUMMARY OF EDGE ROD BOWING RESULTS

	As-Designed Configuration 10-1	Optimized Layout Configuration 10-3
BOL cold		
Number of spacers	10	10
Minimum spacer pitch in core (mm)	150	150
Maximum spacer pitch in core (mm)	300	250
Maximum spacer pitch (mm)	300	400
BOL hot		
Axial elongation (mm)	19.3	19.3
Peak bow toward duct (in core) (mm)	0.06	0.02
Peak bow toward interior (in core) (mm)	0.02	0.015
Peak bow (out of core) (mm)	-0.49	-0.07
Peak stress (MPa)	78.6	83.4
Peak strain (%)	1.1	1.1
Peak spacer reaction force (N)	4.9	6.2
EOL hot		
Axial elongation (mm)	83.8	86.4
Peak bow toward duct (in core) (mm)	(a)	0.90
Peak bow toward interior (in core) (mm)	0.47	1.14
Peak bow (out of core) (mm)	-0.95	-1.13
Peak stress (MPa)	35.7	38.6
Peak Strain (%)	12.8	13.0
Peak spacer reaction force (N)	3.4	3.7
Peak swelling (% $\Delta D/D$)	11.9	11.9

(a) Duct contact at 17,400 hr.

The analysis showed that the current reference design spacer layout is inadequate to achieve the design fuel assembly burnup. An optimized 10-spacer layout was developed which significantly reduced rod deflections. However, there has not been sufficient thermal-hydraulic analysis to establish whether the predicted rod bowing with the optimized spacer layout is acceptable from a thermal-hydraulic standpoint. The criterion for limiting rod deflections was also investigated, and it was found that the current criterion is extremely restrictive and will only allow the assembly to reach about 60% of its design life. Future work will be directed toward (1) establishing a rational limit for edge rod bowing deflections, (2) evaluating the effects of tolerances and duct dilation on edge rod bowing, and (3) evaluating results applicable to designs with larger edge spacings.

2.3. CORE ASSEMBLY STRUCTURAL DESIGN CRITERION

2.3.1. Introduction

The GCFR fuel assembly design criterion (Ref. 2-17) is intended to provide a complete specification of all factors which would influence the design. In particular, thermal and structural limits are provided which ensure safe, reliable operation during normal, upset, and emergency conditions based on applicable work performed in the liquid metal fast breeder reactor (LMFBR) program. The faulted conditions in the GCFR, however, are not similar to those in the LMFBR. There are marked differences in coolant and flow geometry at extremely high temperatures, and there is a possible loss of pressure and flow. Consequently, an identical or extrapolated limit based on LMFBR information is not possible for the GCFR. The LMFBR is currently designed to a faulted temperature limit of 1000°C, which is the onset of sodium boiling. Thus, the LMFBR is not limited by the structural integrity of the material under these conditions, but rather by coolant instability. Such a limit, although probably adequate for GCFR

use, would significantly impact the design of the emergency core cooling systems. Consequently, a new limit which considers the inherent stability of the GCFR coolant has been sought.

2.3.2. Tentative Faulted Temperature Limit

Little is known about the structural properties of stainless steel between 1000° and 1400°C (the melting point of 316 stainless steel). There are some aerospace data for ultimate strength, but these data are subject to large scatter and uncertain testing conditions and probably do not apply to irradiated material. The only information on the structural capabilities of stainless steel for use in fuel rod cladding at extremely high temperatures is Ref. 2-18, which compares Zircaloy and stainless steel at 1200° to 1260°C. This was used as a starting point in establishing a GCFR limit. Reference 2-18 [which is for a light water reactor (LWR)] compares the relative merits of stainless steel and Zircaloy in relation to peak cladding temperature during two typical loss of coolant accident (LOCA) conditions. In both cases, the cladding was assumed to be ruptured, and the tendency of the cladding temperature to approach melting was assessed. Metal-water reaction heat generation played a significant part in driving the cladding temperature to the melting point. Because the GCFR does not have a metal-coolant reaction or internally pressurized fuel rods, it seems that the limit of 1290°C, a value slightly higher than the 1260°C limit used for LWRs, should be justifiable.

In an effort to assess the load-carrying capability of GCFR stainless steel cladding during faulted conditions, the deadweight load-carrying capability of the cladding was computed (Refs. 2-19, 2-20). Based on these calculations, which used unirradiated material data, the limit of 1290°C was found to be well below the temperature for cladding failure under these loads.

2.3.3. Validation Considerations

There are a number of questions concerning the adequacy of 1290°C as a prudent design limit for faulted conditions:

1. Does fast irradiation have a significant effect on the mechanical properties of 316 stainless steel at high temperatures (>1200°C)? If so, is the choice of design limit affected? Data from Hanford Engineering Development Laboratory (HEDL) and information from other sources indicate a pronounced degradation of material properties at high temperatures and fluences ($>10^{23}$ nvt).
2. Does the rate of the transient leading to faulted conditions affect either the structural or thermal-hydraulic stability?
3. Is there a hold time limit at the maximum faulted limit at which creep and plastic instability occur, causing an uncontrollable temperature increase?
4. What amount of either rod-to-rod or rod-to-edge channel flow blockage arising either from bundle distortion or obstruction by foreign material can be tolerated without destroying a coolable configuration?
5. Does rod bundle - edge spacing interaction or rod-spacer interaction cause significant distortion which exceeds the maximum tolerable flow passage perturbation, causing cladding melting?

These and other questions which arise must be adequately answered before the design limit is scrutinized by licensing authorities. Therefore, a comprehensive testing program is being set up to explore each question. The test data together with the background information above should result in the formulation of a verified cladding temperature limit for faulted conditions.

2.4. CORE ASSEMBLY MECHANICAL TESTING

The objective of this task is to conduct mechanical tests of core assembly components and subassemblies to simulate the mechanical loads expected during normal and abnormal reactor operating conditions. The current phase of the assembly mechanical testing program involves testing of fuel assembly components. The preliminary fuel rod - spacer interaction test using single spacer cells and rods was conducted during FY-76. Reproducibility testing of the hexagonal spacer cells was completed, and testing of a new modified hexagonal design was continued during FY-77. The design and procurement of blanket assembly components for testing was initiated, and further mechanical tests on grid spacers are being designed.

2.4.1. Rod-Spacer Interaction Tests

The purpose of prior rod-spacer interaction tests was to evaluate the effect of interacting forces between the fuel rod and the spacers under the mechanical and environmental operating conditions expected in the GCFR. The simulated forces are primarily caused by bowing induced by temperature gradients and irradiation-induced swelling. Reactor operational transients cause relative motion of the rod and spacer, which results in frictional forces; the frictional forces and relative motion cause wear of the rod and spacer pad surfaces. The interaction force is simulated by a deadweight load on a spacer cell resting on a fuel rod. The calculated loads due to rod bowing have always been predicted to be of the order of 5 N. The results of the reproducibility tests using a reference design hexagonal rod spacer indicated that there was no problem due to these loads.

Fabrication of parts for the second phase of rod interaction tests is complete. The rod interaction test equipment was recently moved to consolidate the various parts of core assembly mechanical testing in one laboratory. The test rigs will be rewired and replumbed and the second-phase interaction part installed in test rig 3. In this second test phase, the rod and spacer

are misaligned by a definite amount, resulting in interference and friction forces. The magnitude of the friction or interaction force will be continuously measured; wear will be measured after each test is completed.

2.4.2. Spacer Grid Structural Tests

The test fixture designs for the modified hexagonal spacer structural tests have been completed. The tests will be conducted on the static Instron test machine, which is capable of testing to a force of about 4000 N. The static tests will be conducted in two loading directions: (1) in the axial direction to simulate thermal expansion of fuel rods locked onto spacer cells and (2) in the horizontal direction to simulate bowing interaction loading of spacer cells.

2.5. HEAT TRANSFER AND FLUID FLOW TESTS

The test assembly for the inlet nozzle flow pressure drop test has been assembled and is being prepared for testing. The testing phase has been indefinitely postponed until additional funding becomes available. The parts for the blanket flow control device have been received. The testing phase of this device will take place after the test of the fuel assembly inlet nozzle flow tests. A helium flow test rig is being planned for use in the flow tests of component test assemblies.

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3. PRESSURE EQUALIZATION SYSTEM FOR FUEL (189a No. 00582)

3.1. CORE ASSEMBLY AND PES SEALS

The first set of static adhesion tests was conducted in FY-75 on 316 and 304 stainless steel at various matching cone angles, contact loadings, and surface finishes. This was followed in FY-76 by a second set of tests using materials including couples of Inconel 718 - 316 stainless steel, Inconel 718 - 304 stainless steel, and 304 - 316 stainless steel. The third test phase includes adhesion tests of metal samples coated with hardened surface materials. The simulated grid plate materials are 316 or 304 stainless steel and Stellite-6B tested against simulated fuel assembly samples of 316 stainless steel and Stellite-6B and coatings of chromium carbide, chromium oxide, aluminum oxide, and Stellite-6. The conical surface angle is limited to a 60-deg included angle, and the static loads are 1,334 N and 1,779 N (simulating 13,340-N and 17,790-N clamping loads for a full-size assembly). The samples have been fabricated and characterized, and assembly in the test fixture has begun. The test furnace, consisting of H_2 gas, a moisture supply, and associated instrumentation, has been successfully checked out.

3.2. ANALYSIS, MODELS, AND CODE DEVELOPMENT

The GCFR is designed with pressure-equalized and vented fuel and blanket assemblies. A pressure equalization system (PES) is provided to perform these functions and contains one unit of the helium purification system (HPS). The PES described in Ref. 3-1 is a complex flow network consisting of manifolded fuel rods, fuel and blanket assemblies, monitor lines through which vented fission gases are swept by inflowing coolant

to the HPS unit, and check valves leading to the suctions of the main and auxiliary circulators which power the system.

The development of the transient PES flow code was continued, and several simplifications were made to the transient integration routine, which was placed on a permanent file. The method of supplying the necessary input to the code is currently being developed. This involves modification of the preprocessor of the SINDA code (Ref. 3-2), on which the flow code is based. A scheme has been developed for writing the node-branch connection information from the preprocessor on a mass storage device for later use by the integration routine. Input of the node and branch properties is in progress.

The charcoal trap friction factor - Reynolds number relationship was reevaluated. For a Reynolds number of 2, the Darcy friction factor was found to be $f \sim 10^5$, which is a large number because f for the granular bed charcoal trap is defined in the same way as f for an empty pipe of the same diameter. Two mistakes in a previous correlation were corrected; the revised correlation gave an effective void fraction (from a flow resistance standpoint) of 0.40. Application of the revised correlation to fuel rod conditions showed the pressure drop across the trap to be a factor of 100 smaller than that across the axial blanket for the same mass flow rate, even though $f \sim 10^5$ for the trap. Figure 3-1 compares the new correlation with available experimental data.

3.3. PLATEOUT AND PLUGGING

Volatile fission products, particularly cesium and iodine, vented from the core assemblies and produced by gaseous precursor decay of fission products vented from the core assemblies may plate out on the walls of the monitor lines. These fission products are swept through the monitor

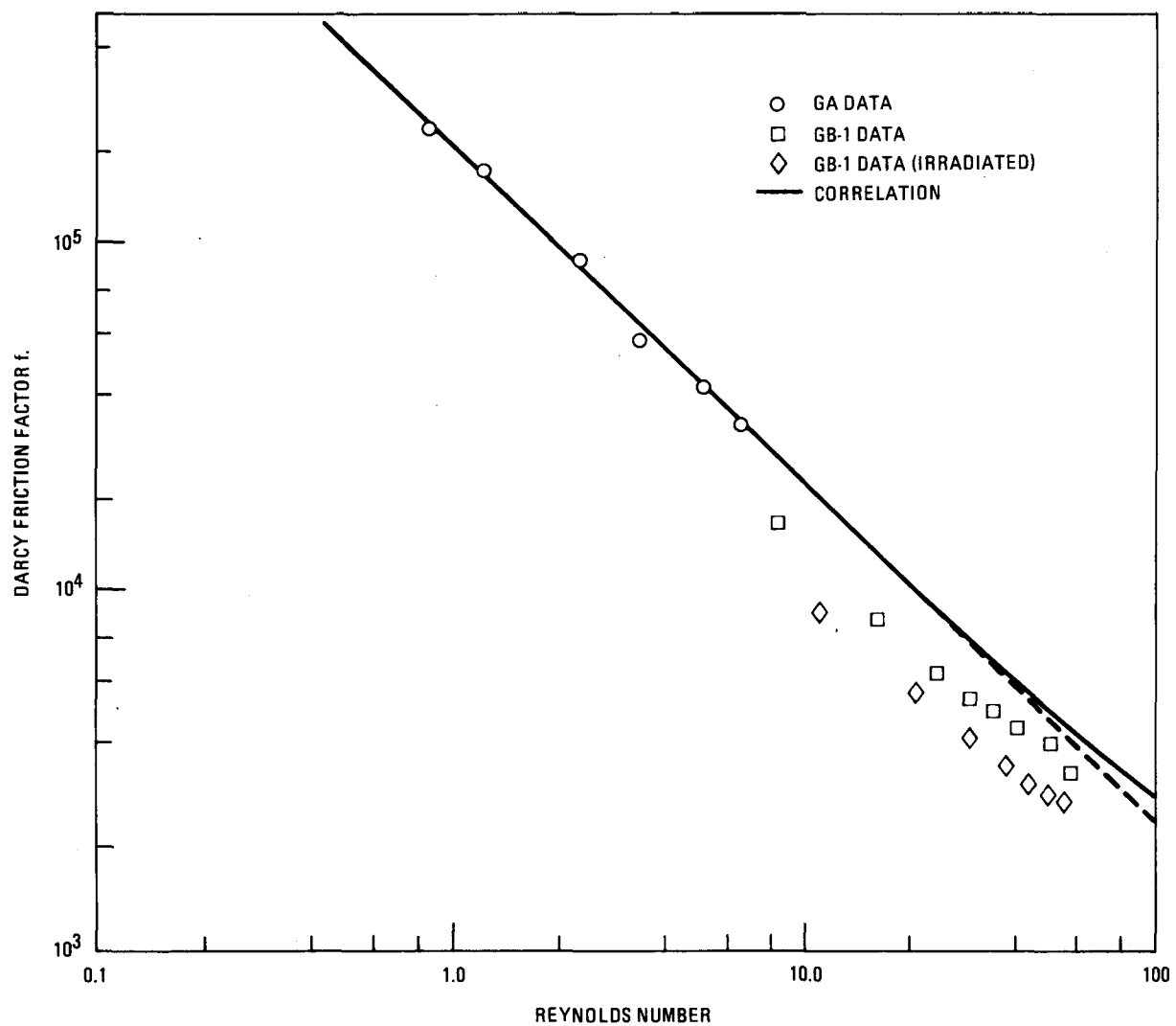


Fig. 3-1. Charcoal trap friction factor

lines into the HPS traps by helium entering at the core subassembly vent connections. Accumulation of deposited material may constrict the sweep gas flow passages and could potentially lead to plugging of the lines. The conditions under which plateout and plugging could occur in the GCFR, the means of minimizing or eliminating them, and the methods for removing deposits are being investigated. A small high-pressure loop has been built and is being used for this purpose. Components for injection, control, and measurement of impurities (i.e., H_2 and H_2O) and sources for simulating venting of volatile fission products and their compounds have been developed.

Two preliminary runs have been carried out using untagged cesium under loop conditions of high and low moisture levels. Since the H_2/H_2O ratio, and thus the prevailing oxygen potential, in the PES is expected to have a strong impact on fission product plateout and the chemical state of the deposited species in the monitor lines, the first runs were carried out at significantly different H_2/H_2O ratios: 1.4 to 1 (total pressure $\sim$ 6.9 MPa; H_2O pressure $\sim$ 690 Pa) and 32 to 1 (total pressure $\sim$ 3.5 MPa; H_2O pressure $\sim$ 10 Pa). The former value is in close agreement with the recommended ratio of 3.0 to 1 for the 300-MW(e) GCFR, and the latter represents a circuit with low oxygen potential. The distribution of cesium in the source tube, bellows-sealed source valve, simulated monitor line, and cesium knockout trap was measured by atomic absorption for both tests. The results indicated that the bellows of the source valve acted as an undesirably large sink for elemental cesium in both runs, with greater than 99% of the source inventory retained upstream of the monitor line test section.

An improved source has been designed that eliminates the valve and its associated high-surface-area bellows. An evacuated, sealed glass ampoule containing elemental cesium is positioned within a 9.53-mm-o.d. stainless steel tube which is approximately 100 mm long and attached to the loop circuit. Surrounding the tube is a 15.9-mm stainless steel

tube wrapped with heating tape and insulation (Fig. 3-2). The latter tube is used to heat the cesium source to the desired temperature while bringing the loop to the prescribed operating conditions. The experiment is initiated by lowering the heating tube and compressing the source tube until the glass ampoule is broken and subsequently returning the heater tube to its raised position. In this way, cesium vapor has a virtually unobstructed path to the flowing loop gas stream, which should increase its transport to the test section.

3.4. FISSION PRODUCT RELEASE AND TRANSPORT

The purpose of this subtask is to obtain experimental data on (1) interdiffusion, (2) gas phase diffusion, and (3) surface back diffusion of gaseous and volatile fission products. The diffusion coefficient data will be used to validate or improve the SLIDER code (Ref. 3-3), a one-dimensional model for fission gas diffusion transport (including radioactive decay). Surface transport and back diffusion data will be used to establish a model for predicting the importance of these mechanisms to contamination of the reactor coolant system.

Studies of the diffusion of Kr-85 in the simulated blanket region of a fuel rod have been completed using a 76- μm -diametral-gap stainless steel rod insert. Results at 0.45 MPa provided a temperature exponent n of 1.66 in the expression $D = D_0 (P_0/P) (T_0/T)^n$, a value in close agreement with the suggested exponent of 1.67 derived from the literature and slightly larger than the value of 1.59 derived using a 127- μm -diametral-gap rod insert. The next series of diffusion experiments will utilize depleted UO_2 pellets in the test section, providing a diametral gap of 140 μm , a value in accord with the demonstration plant GCFR fuel rod design.

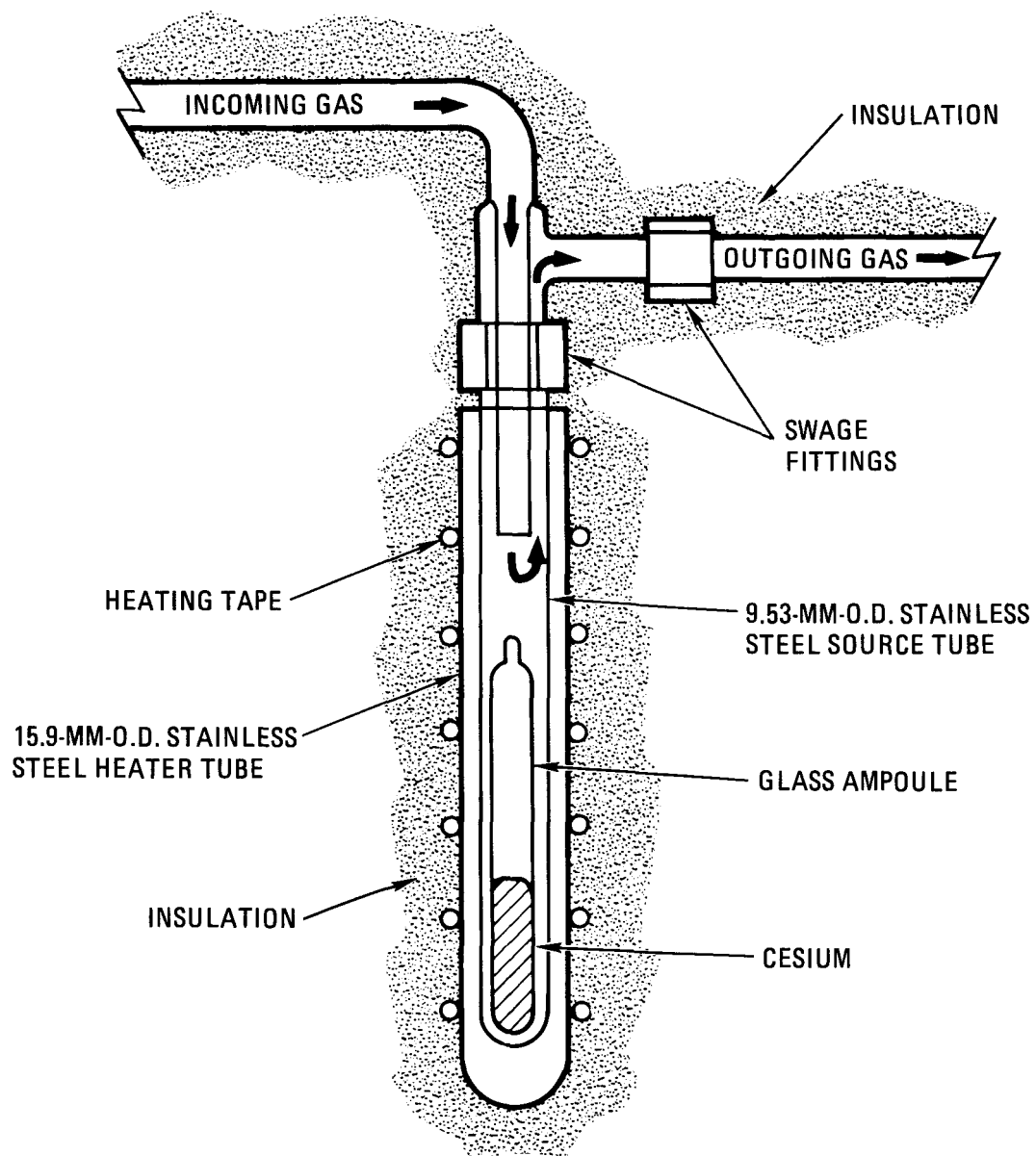


Fig. 3-2. Valveless cesium source for high-pressure plateout and plugging loop

3.5. MONITOR STATION AND INSTRUMENTATION

Research and development is being conducted to establish design criteria and acquire design information which will permit the design of monitor stations for the proposed 300-MW(e) GCFR plant and subsequent commercial GCFR power plants. Design studies conducted in FY-77 have indicated that the complexity of the monitor stations can be greatly reduced and the design and equipment greatly simplified if it is possible to use a small gamma photon detector with a large dynamic range ($>10^5$). Such a detector must also have the capability to resolve photopeaks in a manner comparable to NaI(Tl) in the photon energy range between 80 and 500 keV without the need for cryogenic cooling. During the previous quarter a CdTe(Cl) semiconductor detector was selected (Ref. 3-4).

During this quarter the design and construction of a radiation detector assembly for the helium loop, Mol (HELM) monitor stations were completed, and calibration and testing are in progress. The HELM monitor station will be used on the vent line of HELM, where GCFR vented and pressure-equalized, 12-rod fuel assemblies are being irradiated in the cadmium-filtered flux of the BR-2 reactor. Complementary and more prototypical data will be acquired to augment data previously obtained using a Ge(Li) monitor station on the sweep gas line of irradiation capsule GB-10 at the Oak Ridge Research Reactor (ORR).

3.5.1. Description of HELM Monitor Station

The HELM monitor station will consist of four major elements: (1) gamma radiation detector-spectrometer, (2) electronic data processing and control, (3) teleprinter and data storage, and (4) HELM instrumentation input. The arrangement of the radiation detector assembly is shown in Fig. 3-3 and consists of the CdTe(Cl) semiconductor detector (2 x 2 x 7 mm) and its housing, an electronic preamplifier, a thermoelectric cooler, lead shielding, and mechanical mounting and supports

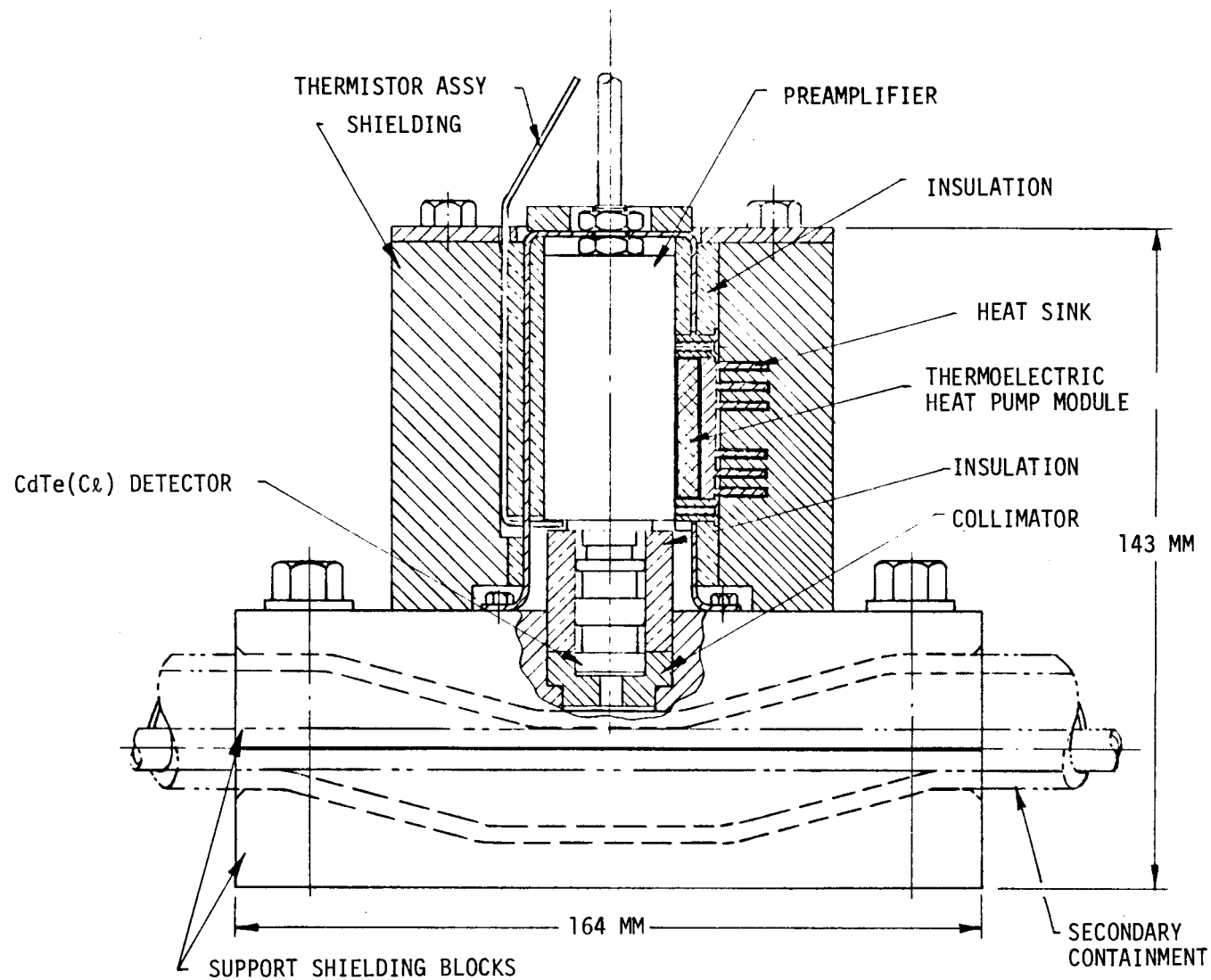


Fig. 3-3. Radiation detector assembly

for installation on the vent line between the fuel bundle (HELM 3) and the fission product storage traps. No penetrations of the vent line or its secondary containment bellows-type metal hose are required. The detector contains a built-in, encapsulated, 20- μ Ci Ba-133 test source.

The electronic cabinet will house the radiation detector power supply, linear amplifier, single-channel analyzer, and detector temperature controller. It will also contain a microprocessor for performing the computations and control functions described in Table 3-1. The electronic circuit arrangement is shown in Fig. 3-4.

A teleprinter console will provide the capability to interrogate the microprocessor, print out data upon request, and record the data on cassette-type magnetic tapes for remote analysis. Data acquisition frequency will be controlled by the microprocessor or "on command."

Because of the late introduction of the monitor station into the HELM installation, existing HELM instrumentation signals will be used as input to the monitor station. The electronic assembly will contain the equipment necessary to match the monitor station to the HELM instrumentation. The signals desired are as follows:

	<u>HELM Instrument No.</u>
HELM 3 bundle power	--
Coolant outlet temperature	1T7b
Sweep gas temperature	2T62
Sweep gas pressure	2P19
Sweep gas flow rate	2F10
BR-2 power	--
Power supply (220 V, 50 Hz, 1 ϕ)	--

The monitor station radiation detector may also be connected to the HELM radiation spectrometer and data reduction system. It could supply

TABLE 3-1
HELM MONITOR STATION PROCESSING AND CONTROL FUNCTIONS

1. Convert photopeak count rates and sweep gas conditions into isotopic activity concentrations and leak rates
2. Correct for dead time during pulse pileups
3. Manually change the selection of photopeaks to be measured
4. Trip high- and low-activity alarms
5. Reject leak alarms during increasing BR-2 or HELM bundle power, changing outlet coolant temperature or depressurization
6. Reset alarm levels following each alarm trip (based on current rates)
7. Provide equipment failure test and/or alarms
8. Store raw and processed data on magnetic tape (digital form) compatible with external data processing and computer equipment
9. Set and change data logging and storage intervals
10. Change data logging and storage intervals upon specifiable changes in signal levels
11. Print out selected data upon interrogation from teleprinter terminal
12. Provide on-line magnetic tape capacity for at least one week
13. Warn of impending expiration of magnetic tape storage capacity
14. Indicate that the detector cooler is in operation
15. Trip detector temperature alarm

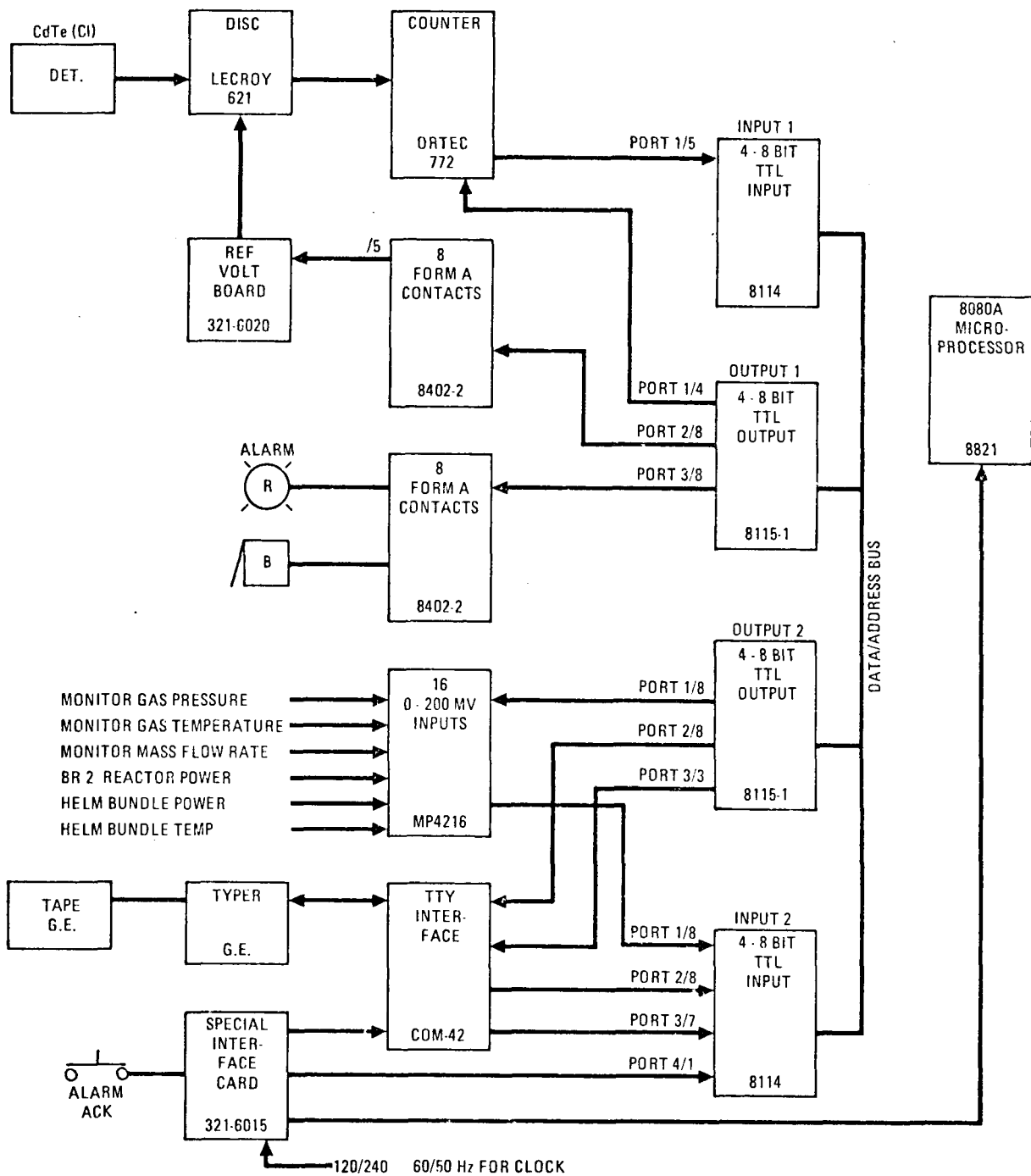


Fig. 3-4. Electronic circuit arrangement

continuous on-line spectral information if there is a period when only intermittent sampling and dilution of gas samples are possible with the Ge(Li) detector. In addition, the performance of the monitor station radiation detector may be better evaluated using the multichannel analyzer capability of the HELM instrumentation. Comparison of the performance of the monitor station with that of the superior resolution of the Ge(Li) detector and the HELM instrumentation will also be beneficial.

3.5.2. Testing and Calibration

The thermoelectric heat pump and heat sink are being tested to ensure that they are adequate to control the temperature of the detector. Although the detector can operate at room temperature or above, it will be located within its own lead shielding, which will be enclosed in a lead tunnel carrying the vent line and secondary containment piping. At the detector location, the vent line is located in a large covered floor well. Thus, it was considered prudent to provide a means of removing Joule heating from detector electronics and dissipating it in the air of the well.

The detector is being calibrated using sections of the 8-mm-o.d. pipe filled with secondary standard liquids. Solutions containing Se-75 and Ba-133 are used to provide the following photopeaks:

<u>Isotope</u>	<u>Photopeak Energy (keV)</u>	<u>Photons/Disintegration (%)</u>
Ba-133	81	36
Se-75	136	56
Se-75	265	59
Se-75	280	25
Ba-133	302	19
Ba-133	356	62

Photopeaks identified from the GB-10 and HELM 2 irradiations as those most likely to be useful for fuel assembly monitoring are shown below.

<u>Isotope</u>	<u>Photopeak Energy (keV)</u>	<u>Photons/Disintegration (%)</u>
Xe-133	81	37
Kr-85m	151	74
Kr-88	196	38
Xe-135	250	91
Kr-87	403	53

The initial calibration tests indicated that the detector was over-calibrated based on data taken during the HELM 2 test with an on-line Ge(Li) detector. Recalibration with more open collimators is in progress. Assembly of the electronic components is expected to be completed during the next quarter.

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4. CORE ASSEMBLY DESIGN VERIFICATION (189a No. 00582)

Core assembly design verification consists of planning and executing the principal GCFR test programs to demonstrate satisfactory performance of the GCFR core assemblies experimentally. Model assemblies will be tested over a range of simulated GCFR environments, and the resultant performance data will be used to establish the GCFR design data base, to verify design analyses and predictive design computer codes, and to explore for design faults. The goals of these activities are to provide assurance of design performance and to generate information necessary to support licensing. The current task scope includes

1. Core flow test loop (CFTL).
2. Prototype assembly tests.
3. Helium flow tests.
4. Depressurized accident condition tests.

The principal accomplishments of this quarter were

1. The CFTL Coordinating Committee [GA, Helium Breeder Associates (HBA), ORNL] met for the first time and agreed on the content of the CFTL management plan, except for the quality assurance requirement.
2. A temperature-pressure instrumentation location scheme for the CFTL 37-rod bundle was completed, and a detailed evaluation of the calibration of fuel rod simulator thermocouples to be used in the 37-rod bundle was completed.

3. The thermal-hydraulic characteristics of a number of spacer/hanger rod sleeve combinations were evaluated for the 37-rod bundle. One particular combination resulted in moderate temperature differences (maximum of 34°C) across rods.
4. The CFTL test specification for the blanket assembly was completed.
5. The conceptual elements for the helium flow test were developed based on the requirements in Ref. 4-1 and the results of air flow tests.

4.1. CORE FLOW TEST LOOP PROGRAM

A series of out-of-pile simulation tests will be performed to (1) demonstrate the ability of the GCFR fuel, control, and blanket assembly designs to meet design goals and (2) verify predictions of analytical models which describe design operation and accident behavior. The emphasis of the tests will be on obtaining thermal-structural data for steady-state, transient, and marginal conditions using electrically heated rod bundles in a dynamic helium loop. Final margin tests will be progressively extended to the highest possible temperature until the heater elements fail. The CFTL program plan (Ref. 4-2) describes the requirements for the test program to be conducted in the CFTL, which will be constructed and operated by ORNL.

4.1.1. Program Planning

4.1.1.1. CFTL Objectives. The CFTL program objectives are given in Ref. 4-2. As the GCFR and the associated CFTL program have evolved, the program objectives have been refined to emphasize general and specific goals. The current objectives are used below. These objectives supersede those in Refs. 4-3 and 4-4.

1. To verify the steady-state and transient analytical methods employed to predict the behavior of GCFR fuel, control, and blanket assemblies and the control rod.
2. To test models of GCFR assemblies under steady-state and transient conditions.
3. To determine unknown core assembly design deficiencies.
4. To verify integral bundle heat transfer and fluid flow performance data obtained from other experimental programs.
5. To verify the analytical methods employed for extrapolation to full-size assemblies.
6. To examine assembly bowing due to thermal gradients across fuel and blanket rods and model bundles. This objective is subject to a cost effectiveness evaluation.
7. Within the limit of the fuel rod simulators, to test and verify the ability to analyze fuel, control, and blanket assemblies under all design basis accident conditions.
8. Within the limit of the fuel rod simulators, to examine bundle behavior beyond the fuel damage limit.
9. To perform exploratory tests of local flow blockage to determine the ability to detect blockage, measure the thermal and hydraulic effects of the blockage, and verify the analytical models used for calculating the effects of the blockage.

4.1.1.2. CFTL Management Plan. The principal elements of the CFTL management plan (Ref. 4-5) are

1. Breakdown of the program by main activities and identification of responsibility.
2. Applicable standards.
3. Functions and responsibilities of organizations.
4. Details of how the program is planned, executed, and documented.

The key features of the management plan are delineated in Fig. 4-1 and Table 4-1.

4.1.1.3. CFTL Coordinating Committee. The CFTL Coordinating Committee is composed of representatives of GA, ORNL, and HBA and is responsible for determining and resolving CFTL issues. The functions of the committee are as follows:

1. Coordination of the responsibilities of the program participants.
2. Review and recommendation of the technical program, budget, and schedule.
3. Provision of assurance that the program meets GCFR development requirements.
4. Provision of information and responsiveness to DOE.
5. Recommendation of revisions, as necessary, of the management plan and quality assurance principles.
6. Modification of the committee's functions and composition as required to accomplish the program objectives.

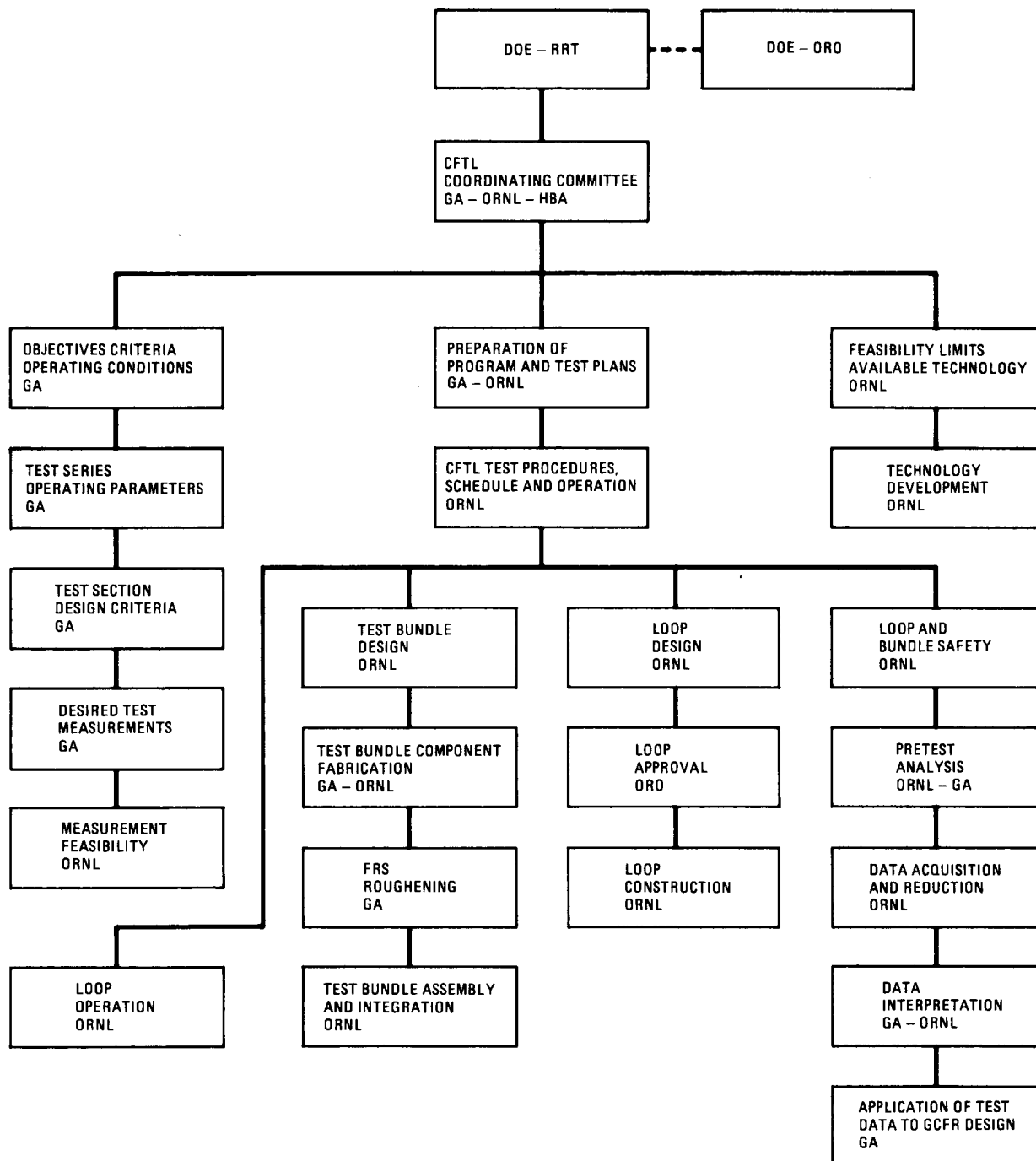


Fig. 4-1. CFTL organizational responsibilities

TABLE 4-1
DOCUMENTATION RESPONSIBILITIES^(a)

	DOE-ORO ^(b)	DOE-HQ ^(c)	ORNL	GA	HBA
<u>Control</u>					
Management plan	R	A	P	I	R
Program plan	I	A	R	P	R
System design description	A	I	P	R	I
Directive	A(P)	I	I		
Work plans and schedules	A	I	P	I	I
Test section design drawings			R	P	I
Division of responsibility	I	I	P/A	P/A	R
<u>General</u>					
Milestones	A	I	P	I	
Meeting agenda, minutes, agreements, and commitments	I/R	P/I/R	P/I/R	P/I/R	P/I/R
Progress reports					
Monthly/quarterly	I	I	P/I	P/I	I
Annual/sesquiannual	I	I	P/I	I	I
Annual list of control documents	I	I	I	P	I
<u>Construction</u>					
Environmental assessment (complete)	A	I	P		
Conceptual design report (complete)	R	R	P	I	
Request for directive (complete)	R	I	P	I	
Quality assurance program plan	R	I	P	I	I
Quarterly project summary	R	I	P	I	I
Quarterly cost and schedule report	R	I	P		
<u>Functional</u>					
Test specification (GA)	I	I	R	P	I
CFTL-189a (ORNL)	R	A	P	I	I
CFTL work plan (GA)	I	A	I	P	I
Operating procedures	I	I	P	R	
Test bundle examination procedures	I	I	P/R	P/R	I
Maintenance procedures	R		P	I	
Test plans and schedules	R	A	P	R	R
Topical reports	I	I	P/I	P/I	I
Quality assurance documentation	R	I	P/I	P/I	I
Critical item reports	R	R	P/I	P/I	(I)
Reduced test data			P	R	
Commitment modification	R	P/R	P/R	P/R	I
Test bundle design drawing	I	I	P	R	I

- (a) P = preparation and approval by preparing party.
R = review; reasonable time allowed for review and comments.
A = approval.
I = information; comments not expected.
P/I/R = preparation, information, or review; meeting agenda, minutes, agreements, and commitments prepared by host.

- (b) ORO = Oak Ridge Operations.

- (c) DOE-HQ = DOE Headquarters.

4.1.1.4. Quality Assurance Program. Coordination of quality assurance planning between GA and ORNL is part of a continuing program. GA previously requested that ORNL assure that design verification testing would be accomplished in accordance with the requirements of 10CFR50, Appendix B. ORNL states that the formal quality assurance program, which is applied to all GCFR research and development work, is fully responsive to the intent of 10CFR50, Appendix B. ORNL is expected to provide GA with a quality assurance program plan by July 1978.

4.1.2. Test Analysis and Prediction

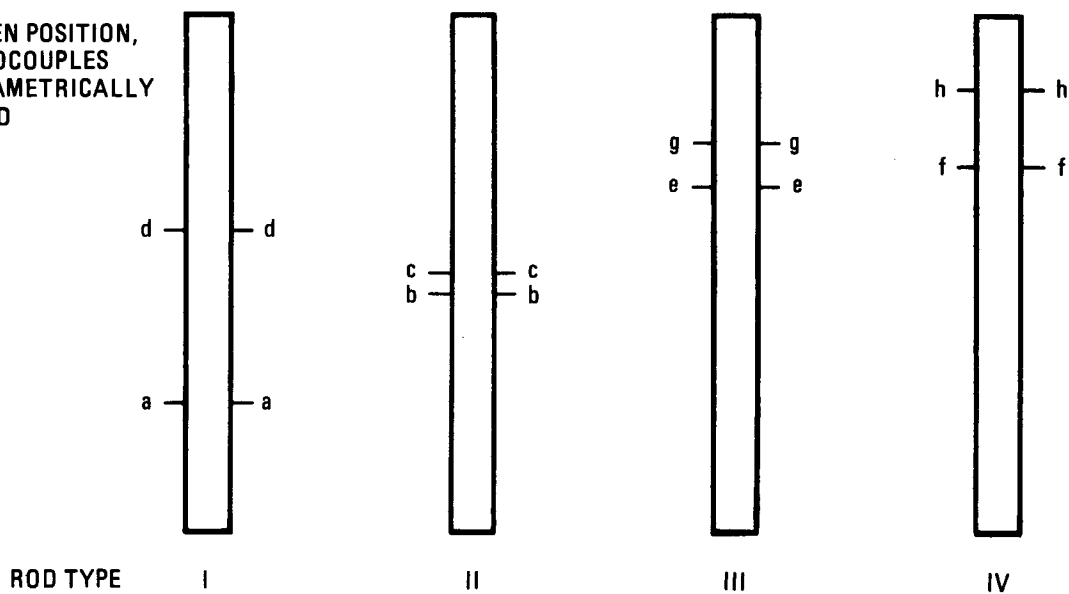
4.1.2.1. Temperature-Pressure Instrumentation Location Scheme. A thermocouple - pressure tap instrumentation location scheme for the CFTL 37-rod bundle was completed. This scheme utilizes four types of rods (each rod is distinguished by the axial locations of four thermocouples 90-deg apart) and has eight different axial positions at which cladding temperature is measured. Two schemes, identified as A and B in Fig. 4-2, were evaluated. Scheme A locates each of two thermocouples (in one axial location) on one rod. This is attractive because it permits direct measurement of the diametral temperature difference on a rod at a single axial location. However, this scheme has the disadvantage that each rod is unique and hence has no commonality with any other rod. Thus, unavailability of a particular replacement rod would result in elimination of temperature readings in the locations served by that particular rod. In scheme B, the two thermocouples (in one axial location) are located on different rods. Although this scheme does not permit direct measurement of temperature difference across a single rod at one axial location, it has the advantage of a fair degree of commonality among the thermocouple locations in the various rods. Thus, fewer spare rods are needed compared with scheme A to obtain comparable coverage. Hence, scheme B was chosen.

Figure 4-3 shows the proposed axial location system for the thermocouples and pressure taps for the CFTL 37-rod bundle. At present, this arrangement is believed to be appropriate for the 61- and 91-rod bundles

NOTE:

AT GIVEN POSITION,
THERMOCOUPLES
ARE DIAMETRICALLY
OPPOSED

SCHEME A



SCHEME B

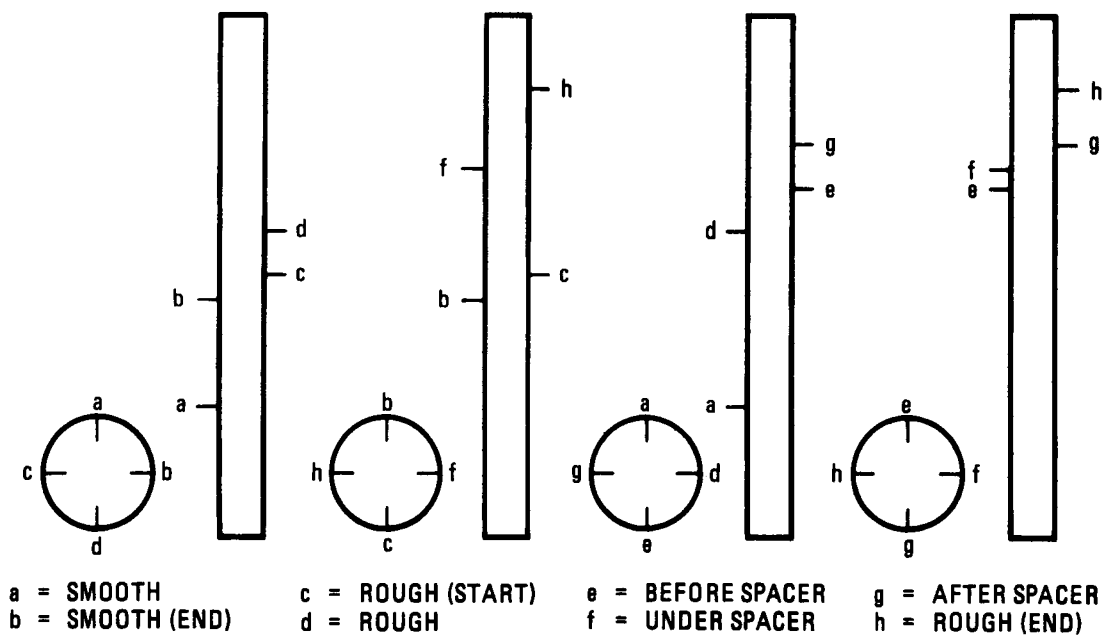


Fig. 4-2. Rod thermocouple location schemes (each letter denotes a thermocouple)

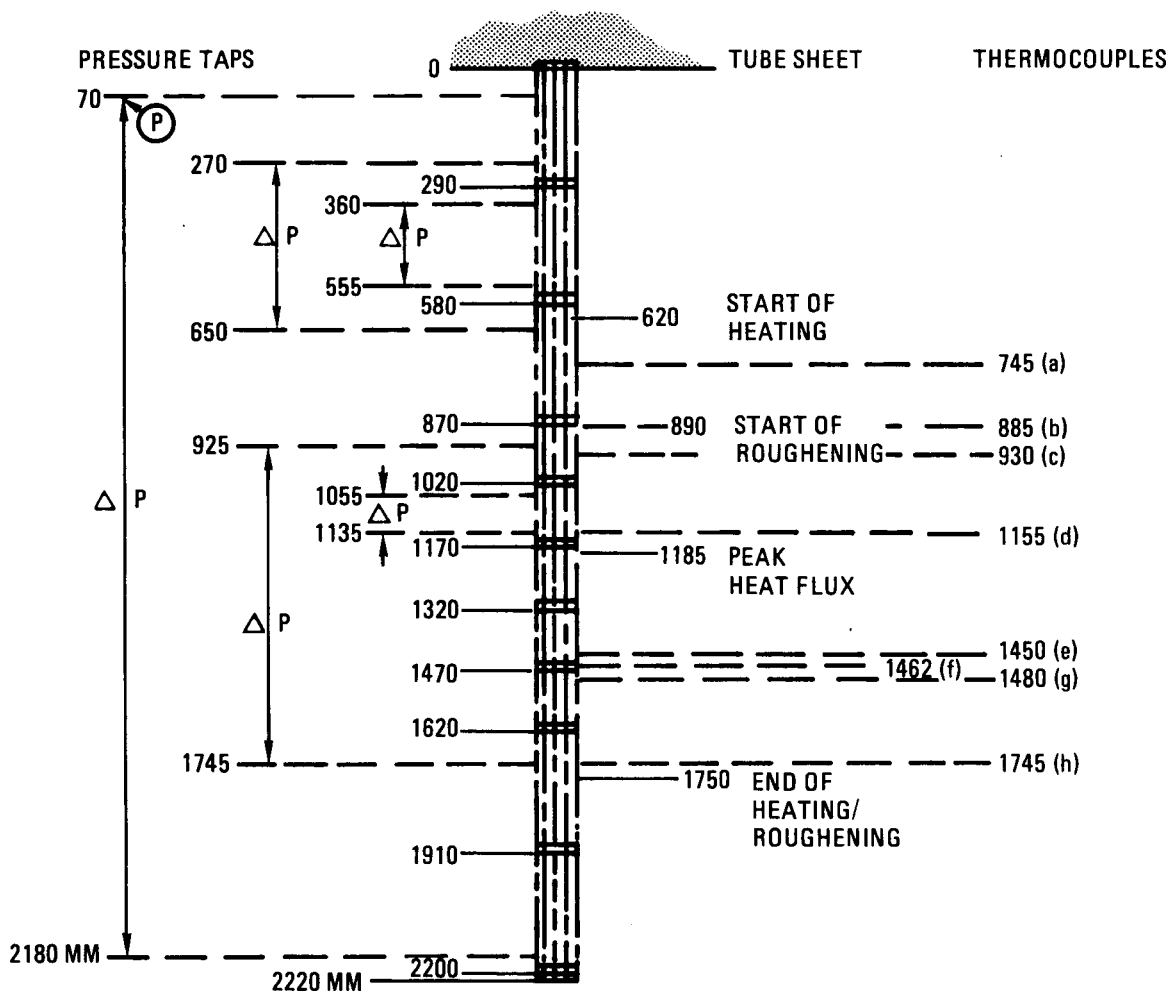


Fig. 4-3. 37-rod CFTL bundle axial instrumentation location system

also. With this arrangement, thermocouples are located near the start of the smooth region; at the end of the smooth region; at the beginning of the rough region; in the highest heat flux region; before, under, and after a spacer in the rough region; and in the highest cladding temperature region. Pressure tap locations are for inlet static pressure, bundle pressure drop, and rough section pressure drop. Figure 4-4 also shows the plan view location of thermocouples in the 37-rod bundle using scheme B (Fig. 4-2); the axial locations are shown in Fig. 4-3. Although some rod arrays investigated had as many as three or four rods of one type (I, II, III, IV) in a row, the preferred rod array shown is such that no more than two rods of any one type are present in any horizontal or diagonal row. The dashed lines identify the types of channels used in the COBRA code (Ref. 4-6). Insofar as possible, edge and edge/hanger rod channel thermocouple locations are in the center of a subchannel, as used in the COBRA code. The locations may be changed as the design of the bundle geometry evolves.

4.1.2.2. Thermocouple Calibration. Detailed evaluation of the calibration of the fuel rod simulator thermocouples to be used in the CFTL 37-rod bundle was completed. In addition to the present thermocouple type and location (sheathed Inconel 600 with an insulated junction and located in the boron nitride insulation), two alternate thermocouple types and placements have been considered. These alternatives have the advantage of not requiring a relatively costly temperature difference/heat flux calibration, which is necessary for the present thermocouple type and placement. The cost and accuracy of possible thermocouple calibration methods are being evaluated for the present and alternate thermocouple types and placements.

4.1.2.3. Application of COBRA Code. COBRA-SIDE runs were made for the design in which the smooth circular hanger rod sleeve diameter is such that the gaps between the hanger rod and adjacent fuel rods are equal and virtually identical to the fuel rod-to-duct-wall gap. Figure 4-5 shows the channel arrangement for the one-sixth section used in the analysis. To

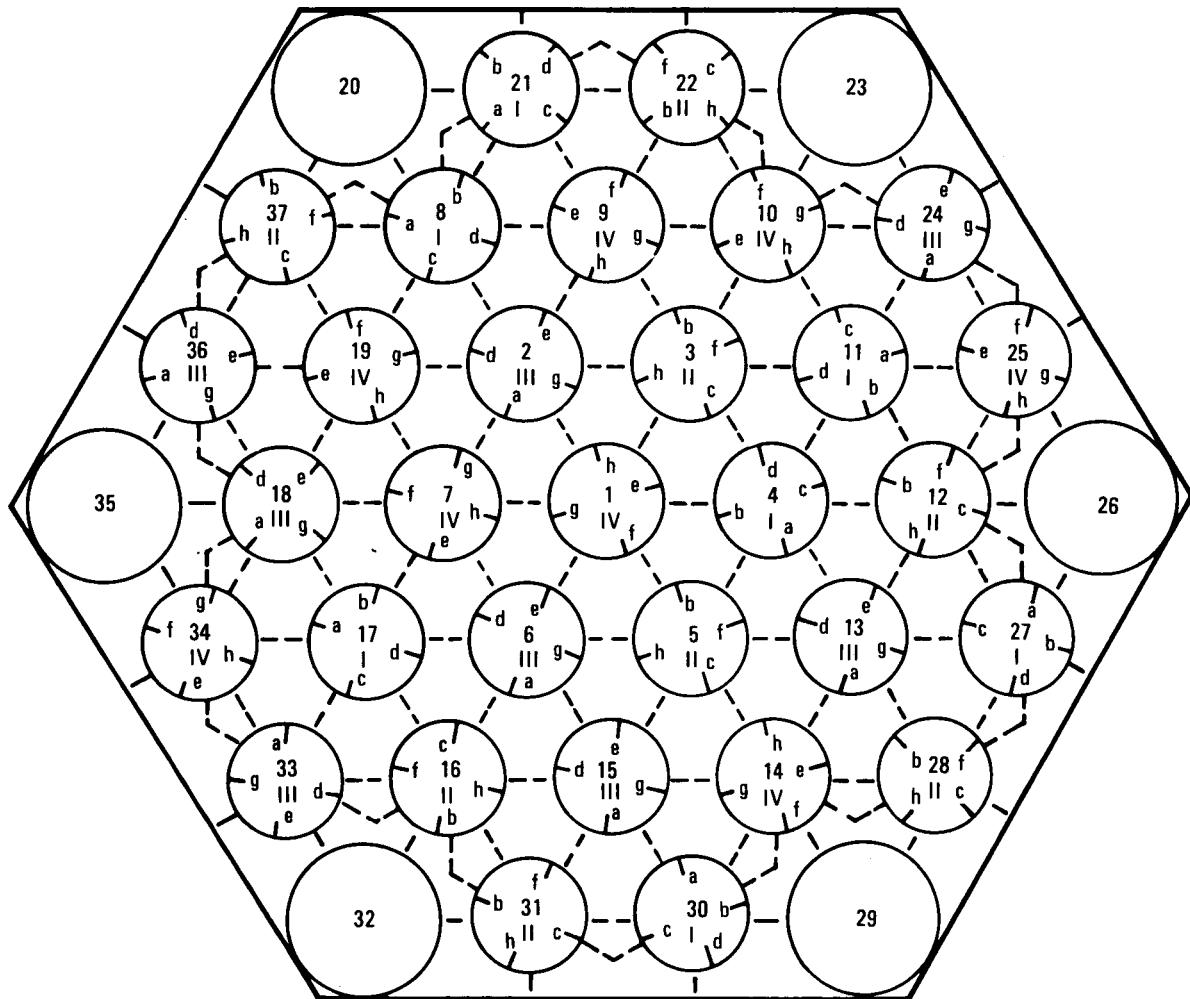


Fig. 4-4. Thermocouple location scheme for 37-rod CFTL bundle (hanger rods 20, 23, 26, 29, 32, 35)

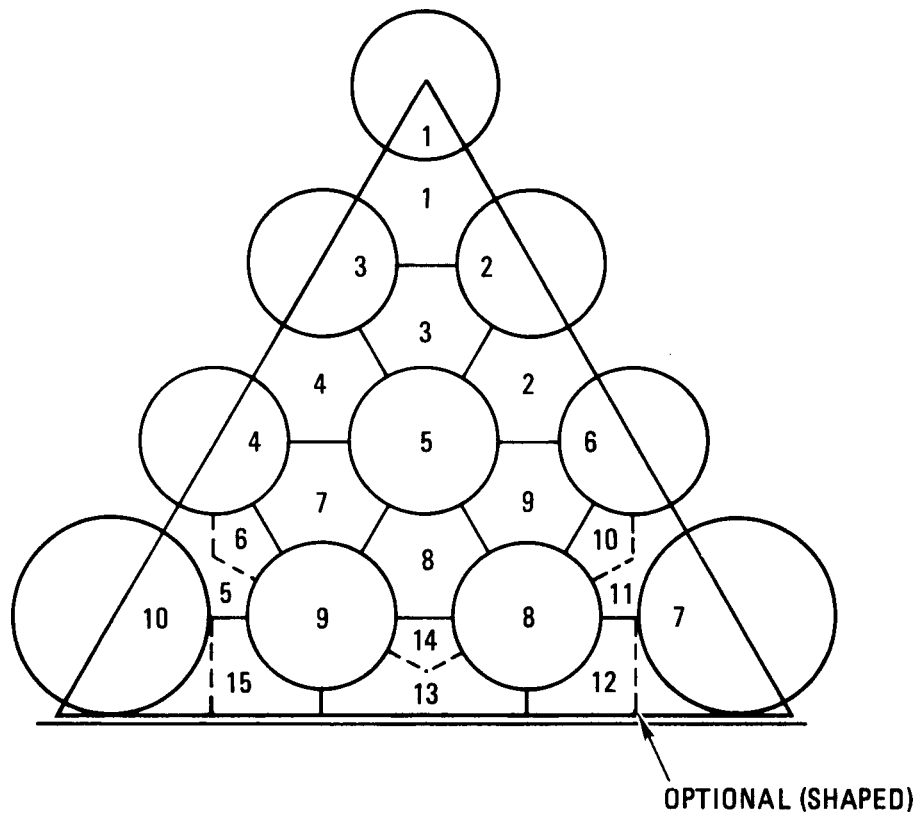


Fig. 4-5. 37-rod CFTL bundle channel layout, one-sixth section

explore the effect of spacers on rod cladding temperature distribution at 100% power/flow conditions, code runs were made without spacers, with current design spacers, and with spacers having a very low loss coefficient in internal channels and a relatively high loss coefficient in edge channels. Typically, for all combinations, the cladding temperature distribution shows that up to the start of the roughened section, the edge/hanger rod sleeve channel has the highest temperature, whereas the diametrically opposed interior channel has the lowest temperature. However, by the end of the roughened section, a reversal of the hottest/coldest channel takes place. Rod bundles without spacers or with design spacers have very similar cladding temperature differences along the rod length. However, the combination of internal channel low loss coefficient and edge channel high loss coefficient results in a substantial decrease in the cladding temperature difference at the end of the rough section, but with a significant increase in cladding temperature difference at the end of the smooth section.

To overcome these shortcomings, a design with a shaped hanger rod sleeve (formed by extending a tangent to the circular portion normal to the duct wall) and two types of spacers (normal blockage in channel 13 in the smooth portion, high blockage in the same channel in the rough section) was evaluated. Figure 4-6 shows that the cladding temperatures for this design are such that the maximum temperature difference is 34°C, with less than 28°C over most of the length.

To determine the effect of the entrance length ahead of the bundle on rod cladding temperatures, COBRA runs were initially made with uniform mass velocities in each channel and then with unequal mass velocities in the channels corresponding to the equilibrium flow distribution. For the latter condition, maximum temperature increases of 3.3°C in the smooth portion and 1.1°C in the rough portion were observed for an edge rod. Since this comparison was made without spacers, even smaller differences would be expected with spacers.

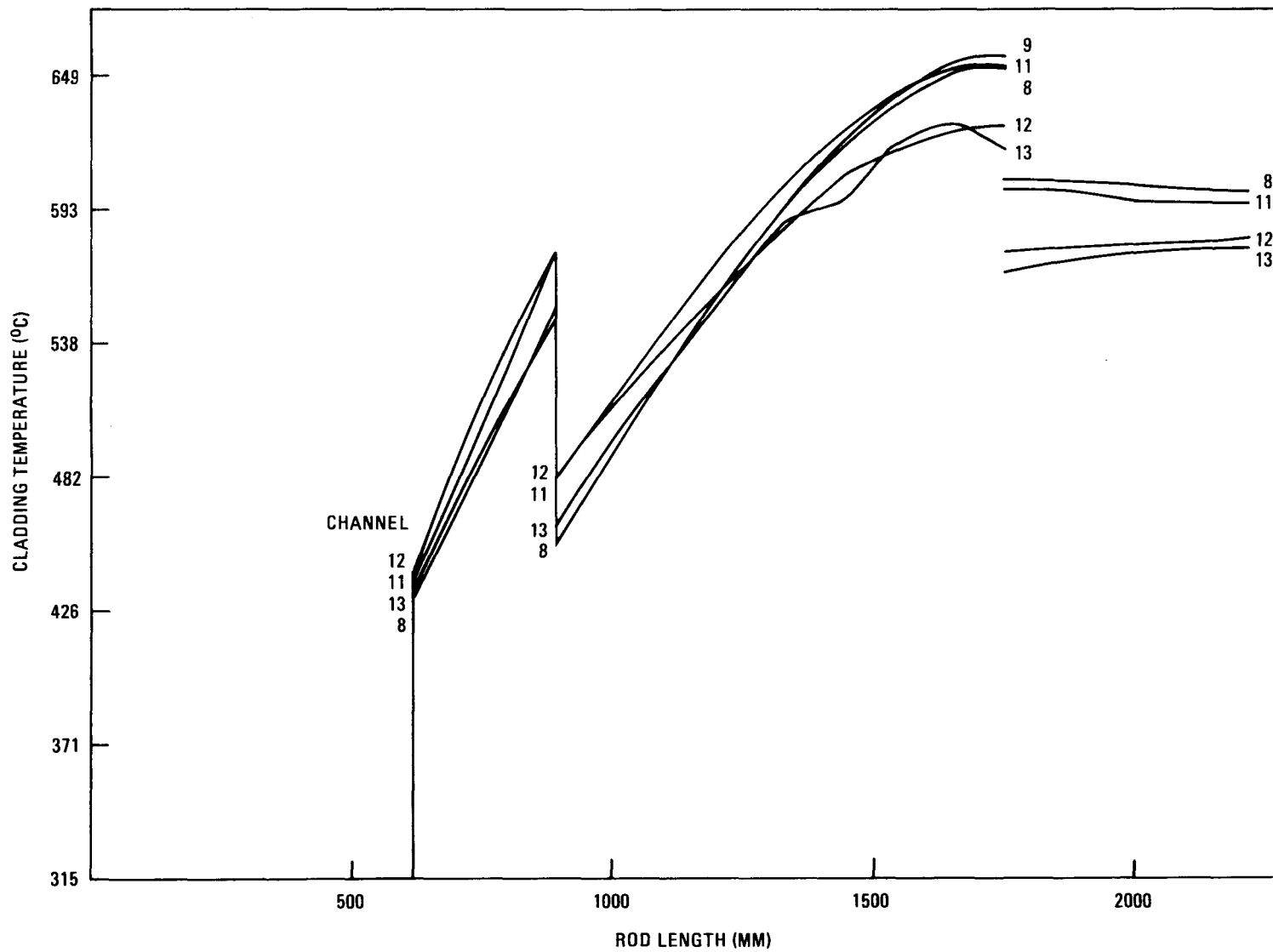


Fig. 4-6. Cladding temperature vs rod length (rod 8) for 37-rod CFTL bundle (shaped hanger rod sleeve, two spacer types)

4.1.3. Test Specification

The test specification for the blanket assembly was completed. The specification covers the objectives and test conditions of rod simulator power, bundle helium flow, and bundle pressure for modeling steady-state and transient operating conditions applicable to the GCFR core radial blanket assembly. Table 4-2 identifies the test series/group numbers and types of tests associated with each group.

4.1.4. Test Bundle Design

The rod simulator locations in each test bundle were identified. Figure 4-7 shows the numbering system applied to rod simulator fuel and control assemblies. Fuel assembly numbering may be applied to any fuel or blanket assembly up to 91 rods.

4.1.5. Liaison

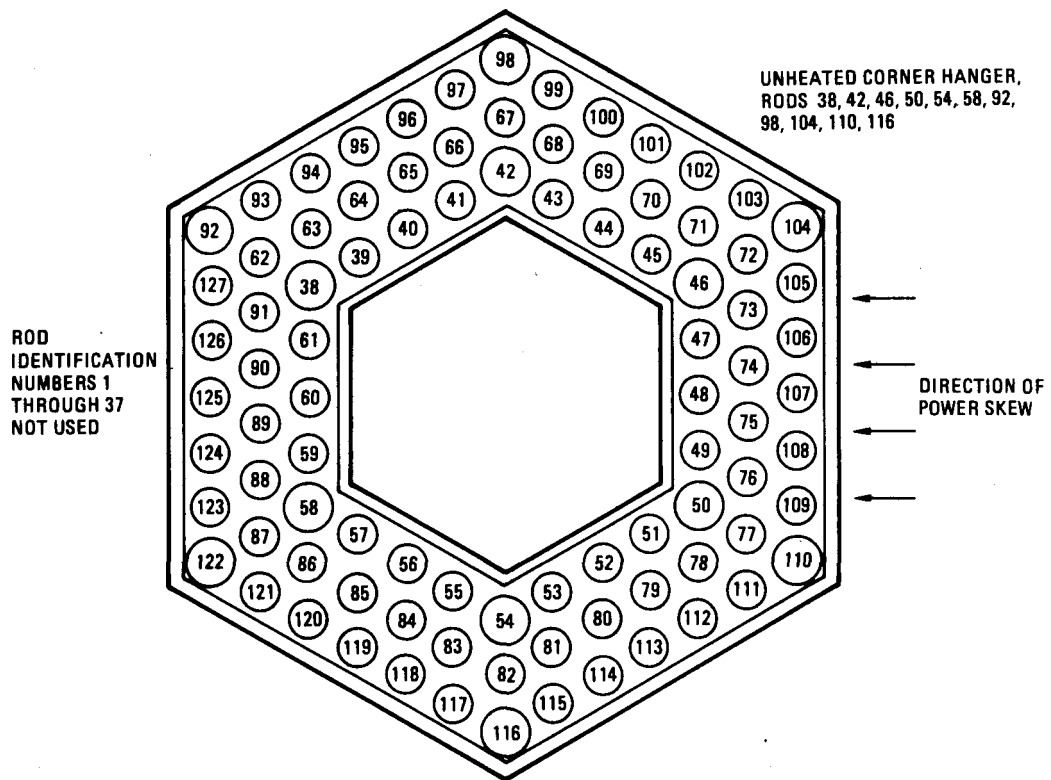
The liaison activity has expanded to include HBA, which is now involved in review and evaluation of the CFTL program. Primary liaison is with ORNL and HBA, and secondary liaison is with other national laboratories, DOE, the Program Review Committee (Fuels), and European GCFR participants.

4.2. GCFR PROTOTYPE ASSEMBLY TEST PROGRAM

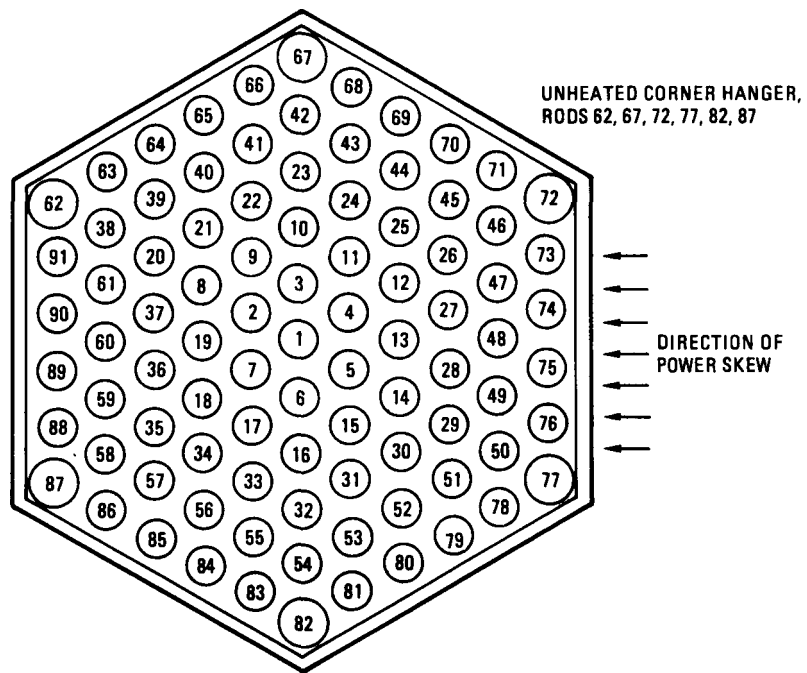
Program planning for testing of the prototype core assemblies is continuing. The tests will be conducted on full-size core assemblies to ensure that they meet design qualification requirements prior to fabrication of the demonstration plant initial core. The prototype assemblies will be the same as the GCFR demonstration plant core assemblies except that the $\text{PuO}_2\text{-UO}_2$ fuel in the GCFR fuel rods will be simulated by depleted UO_2 . The assemblies will be subjected to maximum GCFR helium

TABLE 4-2
BLANKET ASSEMBLY TEST SERIES MATRIX

Test Series/Group Number	Isothermal (Unheated Rods)	Steady-State Flow	Normal Operation	Transition Flow	Laminar Flow	Heated	Uniform Power	Skewed Power	Single Heated Rod	Normal Transients	Upset Transients	Emergency Transients	Power Transients	Flow Transients	Pressure Transients	Overcooling	Undercooling	Design Margin
B1-1.1	X	X	X	X	X													
B1-2.1		X	X	X	X	X												
B1-2.2		X				X		X										
B1-2.4						X		X	X				X	X				
B1-2.5						X		X		X			X	X				
B1-2.6						X		X							X			
B1-2.7		X				X		X								X	X	X



(a)



(b)

Fig. 4-7. CFTL rod simulator identification: (a) 90-rod control assembly, (b) 91-rod fuel assembly

flow conditions to closely simulate the reactor core environment; however, there will be no radiation. One assembly of each type (fuel, control, and blanket) will be subjected to the equivalent of approximately one year each of reactor operation in a hot helium test loop. The helium test loop temperature will be maintained external to the test section, since fuel rod heating will not be simulated in these tests.

The test facility and operation are planned as part of the German contribution to the GCFR program. Several other options are also being considered, including (1) a new facility which would most likely be situated in Germany; (2) the CARMEN-2 loop at Saclay, France; (3) the EBOR loop at Idaho Nuclear Engineering Laboratory (INEL); and (4) upgrading of the proposed GCFR core assembly helium flow test rig. EG&G completed a preliminary proposal based on Ref. 4-7 for conducting the prototype tests in the modified EBOR loop. Recent information indicates that a portion of the EBOR loop components were removed and have either been disposed of or placed in storage, necessitating withdrawal of EG&G's proposal for the EBOR facility to be used for the prototype tests. The Commissariat à l'Energie Atomique (CEA) has indicated that with minimal loop changes, the CARMEN-2 loop can provide a helium flow of 8 kg/s with a test assembly Δp of 290 kPa at 75 bar and 550°C. With additional changes, particularly upgrading of the recuperator/reheater and the test vessel, the loop would meet the desired prototype test condition requirements of 8 kg/s with a Δp of 290 kPa at 90 bar and 450°C.

A feasibility study on upgrading the proposed GCFR core assembly helium flow test rig to permit prototype testing has been initiated. The proposed helium flow test rig (which will be located in the U. S.) will be capable of relatively low pressure and temperature test conditions and will be used for design evaluation testing of full-size core assemblies. The design evaluation studies will include vibration, acoustic, and pressure drop testing. Upgrading would include raising the system pressure from ~6 to 9 MPa and raising the test assembly temperature from ~175° to

450°C or 550°C. This may be accomplished by using the same circulator used for the helium flow tests, but a helium heater and a regenerator/recuperator would have to be added to the system to attain the desired prototype assembly test conditions.

Germany has agreed to complete an engineering design assessment of the prototype facility by mid-1978 so that the funding and schedule requirements necessary for the program can be established. Implementation requirements will be defined by the Fuels, Materials, and Core Components Technical Coordinating Committee under the umbrella agreement, and a test site will be selected. The prototype test schedule is being interfaced with other GCFR program schedules to assure its compatibility with the overall GCFR schedule.

4.3. HELIUM FLOW TEST

Helium flow testing is planned for the design evaluation of full-size core assemblies. Reference 4-1 describes the need for conducting flow tests on the GCFR core assemblies. The objectives of the test are to determine local and overall pressure drops and local flow distributions and to explore acoustic excitation phenomena and flow-induced vibration characteristics. The results of these tests will be used in establishing the satisfactory flow performance of the core assemblies. Therefore, it is required that these tests be accomplished during the core assembly preliminary design phase to provide input for completing the preliminary core design effort. The schedule for these tests is shown in Fig. 4-8. Figure 4-9 shows a simplified flow diagram of the test assembly, including the major test parameters.

4.4. DEPRESSURIZED ACCIDENT CONDITION TEST

The depressurized accident condition test (DACT) will provide core cooling safety margin information under low-pressure, pseudo-steady-state

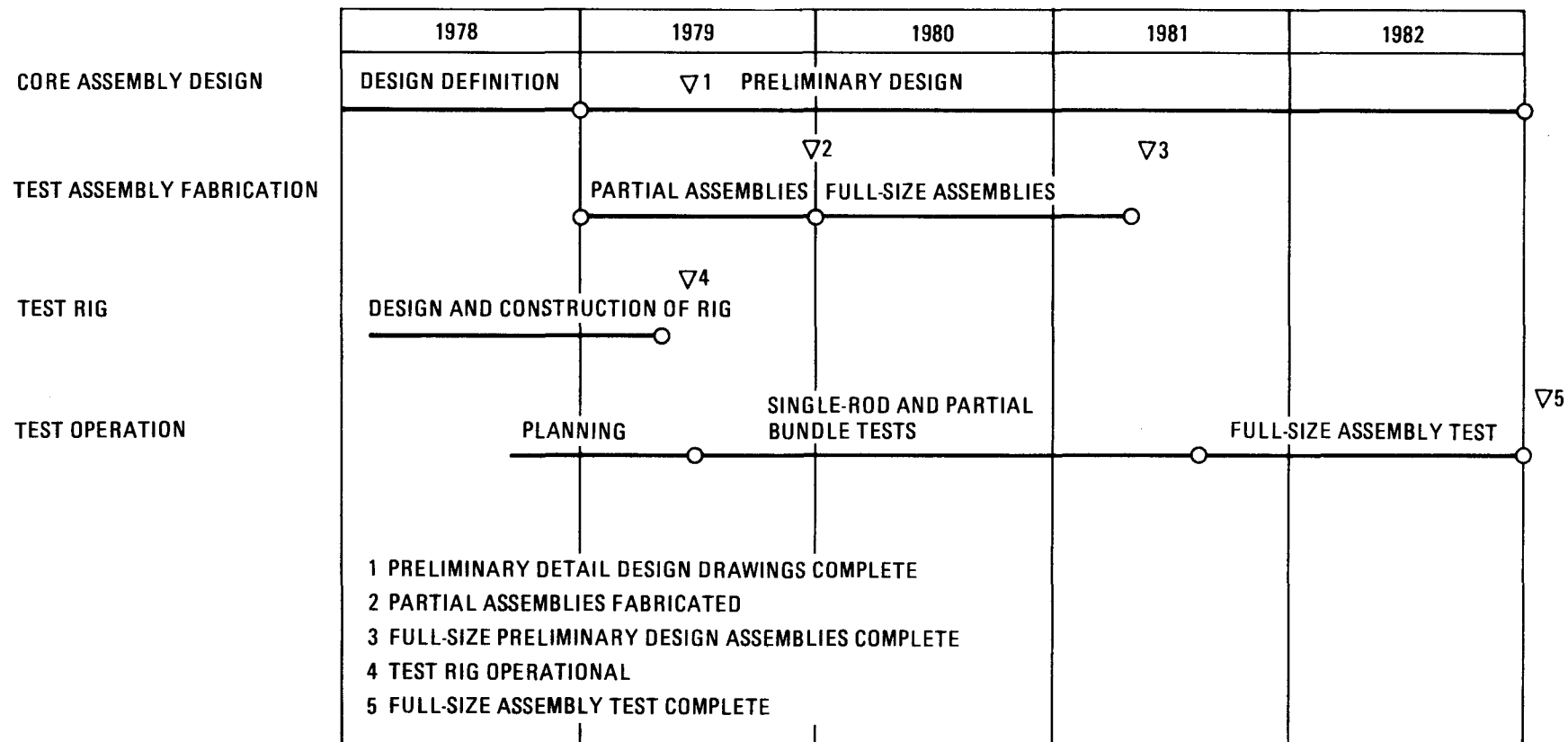


Fig. 4-8. GCFR core assembly helium flow test schedule

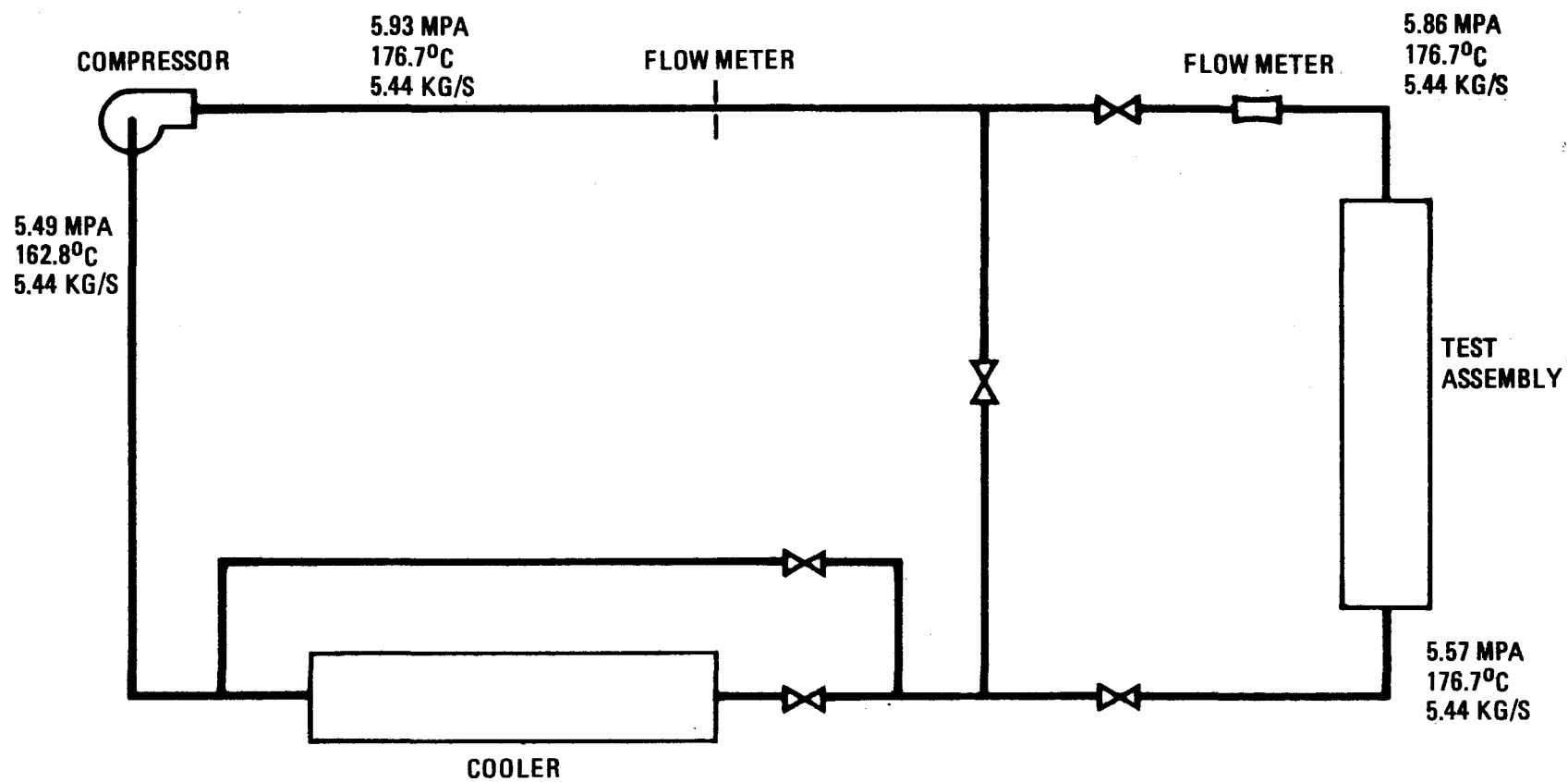


Fig. 4-9. GCFR core assembly helium flow test rig

DBDA conditions. The test is designed to provide experimental information on potential GCFR core assembly edge channel problems and flow and temperature distributions within the bundle under depressurized conditions. In addition, DACT will be capable of investigating natural convection effects within the core assemblies. Since recently developed experimental equipment for the DMFT can be adapted to perform DACT, it is proposed that DACT be conducted at Los Alamos Scientific Laboratory (LASL). LASL is including the DACT requirements in the design of the DMFT test vessel. DACT will be run in FY-79 and FY-80 and thus will be about four years ahead of the CFTL DBDA tests and will provide the near-term information required for licensing reviews.

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5. FUELS AND MATERIAL ENGINEERING (189a No. 00582)

5.1. OXIDE FUEL AND BLANKET TECHNOLOGY

The purposes of this subtask are to (1) maintain liaison with and surveillance of other DOE and non-DOE programs, especially the LMFBR program, to enable utilization of the information on fuel and blanket materials obtained in these programs in the GCFR design; (2) participate in the fast breeder reactor Fuel Element Development Working Group and its subsidiary task groups; and (3) guide the laboratory and irradiation test programs for the GCFR.

During the present quarter, GCFR program representatives met with the Fuel Properties Task Group. The goals of this meeting were to (1) develop procedures for incorporating fuel property data from various reference documents into the Nuclear Systems Materials Handbook (Ref. 5-1); (2) develop a list of important material requirements and priorities for obtaining these data, and (3) compile an inventory of property measurement facilities at the participating sites and determine their status.

Procedures were developed for incorporation of the data from Refs. 5-2 and 5-3 into Ref. 5-1. A list of important physical property data for oxide fuels in the reference fuel system and for alternate fuel cycles was prepared. This list was limited to oxide fuels, since advanced carbide, nitride, and metal fuel materials have not yet been evaluated for use in GCFRs. Priorities were assigned to the data requirements, emphasizing the importance of the Pu/Th system as the prime alternate fuel cycle. The physical property measurement capabilities currently available at GA were also provided.

5.2. CLADDING TECHNOLOGY

5.2.1. ANL Mechanical Testing Program

The objective of the ANL test program is to determine the effects of the following factors on the behavior and properties of GCFR ribbed and smooth cladding:

1. Ribs, rib geometry, fabrication technique, and stress state.
2. Helium impurity levels typical of the environment expected in the GCFR demonstration plant.

These tests are biaxial creep rupture tests with a hoop to axial tensile stress ratio of 2. Tests at a hoop to axial tensile stress ratio of unity and pure tensile tests are planned in support of the irradiated cladding test program.

At ANL, two tests at 650°C and a hoop stress of ~ 238 MPa in a purified helium atmosphere (i.e., O_2 partial pressure of $<10^{-1}$ Pa) have been completed. In general, the ribs increased the rupture life and did not affect strain at failure. The third biaxial creep rupture test (ANL-III) in helium containing 300 Pa of hydrogen and 30 Pa of H_2O is essentially complete; only four specimens, which were added at a later time, are still undergoing testing. Initial analysis of the results indicates that the ribs increased the rupture life. All the tests were performed at a hoop to axial tensile stress ratio of two. Test ANL-IV, specifically designed to separate the effects of the environment and the heat of material, will include some specimens at a hoop to axial tensile stress ratio of unity. ANL will initiate these tests by June 1978.

5.2.2. Pacific Northwest Laboratory Helium Loop Test Program

The primary objective of the Pacific Northwest Laboratory (PNL) helium loop test program is to compare the mechanical properties of the

GCFR cladding in recirculating helium determined at PNL with those in quasistatic helium determined at ANL. The loop has been modified to include moisture sensing instrumentation, and the first test has been initiated. There were significant problems during the first 100 hr of the test, one of which was calibration of the equipment. Efforts to calibrate the equipment were unsuccessful owing to poor performance of the aged gas chromatograph.

During this quarter, gas chromatographs were evaluated based on manufacturers' performance claims and, in some cases, GA experience. Tracor Model MT-150G was evaluated using a calibration gas bottle; this gas chromatograph has been purchased and is being installed in the gas loop.

5.3. F-1 (X094) FAST FLUX IRRADIATION EXPERIMENT

The encapsulated fuel rods in the F-1 (X094) experiment received burnup exposures up to 121 MWd/kg (~ 13.0 at. %, 8×10^{26} n/m²) total fluence and 6.1×10^{26} n/m² ($E > 0.1$ MeV). Postirradiation examination of the seven fuel rods removed at the termination of the experiment is continuing at ANL and GA. At ANL, three of the seven rods (G-4, G-8, and G-9) have been sectioned, and samples have been distributed for burnup analyses, fission product isotopic profile determinations near the fuel-blanket interface, and metallographic examination. Analyses of the fission gases collected from rods G-10, G-11, G-12, and G-13 have been completed, and the results are consistent with previously observed composition values (Ref. 5-4). Recovery of fission gases during puncturing was nonquantitative, indicating possible retention of the gases in the fuel, blanket, or charcoal trap regions of the rod.

All components of the F-1 rods designated for postirradiation examination at GA have been received and processed at the GA hot cell. Five of the twelve charcoal traps released fission gas (Kr-85) to the stack monitor during sectioning, indicating that the nonquantitative

recovery of fission gases from the rod punctured at the ANL hot cell was due, at least in part, to fission gas sorption in the charcoal traps.

Active fission product trap charcoal from F-1 fuel rods G-4, G-9, G-10, G-11, G-12, and G-13 was quantitatively recovered. Shifting of the spacer screens was noted in all cases, confirming an earlier observation made on radiographic prints of the traps. Retrieval of the granular charcoal from each trap proceeded smoothly except for the upper G-4 trap. The charcoal from the two axial compartments closest to the fuel region was agglomerated into a single solid cylinder which was very difficult to remove from the cladding. The initial investigation indicates that the epoxy sealant used during the rod disassembly as the bonding agent was not present. Fission product compounds are considered to be probable candidates since the cesium loading appears to be quite high. The cesium gradients, the retentivity, and the residual sorptive capacity of the trap have become important quantities, although the apparent high loadings were attained under nonprototypical conditions. Using cesium should enable assessment of the performance of the charcoal under loadings which considerably exceed those expected in the GCFR.

5.4. F-3 (X206) FAST FLUX IRRADIATION EXPERIMENT

There was no activity on this subtask during this quarter.

5.5. F-5 (X317) GRID-SPACED FUEL ROD BUNDLE FAST FLUX IRRADIATION EXPERIMENT

As reported previously (Ref. 5-5), the F-5 experiment was designed to study the performance of fuel rods irradiated under simulated GCFR conditions to determine (1) the reliability of the GCFR design and (2) the effect of a step power increase which simulates a 180-deg rotation of a subassembly at the core-blanket interface in the proposed GCFR demonstration plant. During this quarter, the experiment data package was approved, flow testing and assembly of the F-5 (X317) grid-spaced fuel rod bundle experiment was completed, and irradiation was begun in EBR-II run 93. The

experiment has achieved a burnup of 7 MWd/kg (0.8 at. %), with a goal of 48 MWd/kg (5.3 at. %) for the first phase of its irradiation.

The exposure goal for the initial phase of the F-5 irradiation is being reevaluated. LIFE-III (Ref. 5-6) calculations being independently carried out at ANL and GA are yielding discordant results. ANL calculates a plenum pressure of 3.6 MPa, and GA calculates a plenum pressure of 3.06 MPa in the F-5 fuel rods at 48 MWd/kg (5.3 at. %) burnup. The plenum pressure limit set by EBR-II for row 5 positions adjacent to control rods is 3.06 MPa. The calculational input data used by ANL and GA are being compared.

A revised and corrected tabulation of the chemical analyses for the F-5 (X317) grid-spaced bundle components was submitted to the EBR-II project as an addendum to the as-built quality assurance documentation. Post-run information for EBR-II runs 93A and 93B indicate that the F-5 fuel rods are operating at a maximum linear heat rating of 404 W/cm, which is within design tolerance. In addition, analysis of the effect of phase II natural circulation plant tests (to be run at the end of EBR-II run 93) indicated that there will be no problems.

5.6. GB-10 VENTED FUEL ROD EXPERIMENT

During this quarter, destructive examination of GB-10 vented fuel rod GA-21, which achieved an exposure of ~112 MWd/kg in the Oak Ridge reactor, continued at ANL. Postirradiation examination of the GB-10 fission product trap was initiated, and the charcoal was semiquantitatively divided into 10 separate samples and assayed by gamma ray spectral analyses using the hot cell GeLi(γ) detector. Preliminary estimates indicate very high gamma activity levels consisting of only Cs-137 and Cs-134. Of an original charcoal mass of 461 mg, 350 mg, or 76%, of the charcoal were recovered. Note that the trap was opened and unloaded at ANL. The fraction retained by ANL is approximately one-fourth of the total, which will be quantified prior to analysis. The results of spectral analysis are given

in Table 5-1. The cesium isotope levels reported in Table 5-1 normalized to the original trap loading of 461 mg translate to 9.56 mg Cs-137, which represents 2.2% of the Cs-137 inventory. This Cs-137 level is substantially higher than that of any previously examined fission product trap, and particular attention will be directed toward detection of other volatile fission products during subsequent tests.

5.7. HEDL CLADDING IRRADIATIONS

The objectives of the GCFR cladding irradiation and postirradiation test program are as follows:

1. To determine if the mechanical behavior of ribbed cladding is different from that of irradiated smooth cladding under biaxial loading conditions.
2. To determine the effect of stress state on rupture life and creep ductility.
3. Using a statistically designed test program, to determine the quantitative effect of ribs and stress state on creep rupture behavior.

Some ribbed cladding specimens have been irradiated and are at ANL for postirradiation testing. A request for approval-in-principle for irradiation of unstressed specimens of GCFR ribbed and smooth cladding was approved by DOE. Recent data from the reference cladding and duct program indicate that the postirradiation creep rupture lives are lower than in-reactor creep rupture lives. In other words, the postirradiation creep data are overly pessimistic for design purposes. Therefore, it may be necessary to use prepressurized specimens for irradiation. In-reactor creep will be measured during scheduled interim examinations, and the test matrix will have to be modified (this task is being undertaken by ANL).

TABLE 5-1
GB-10 CHARCOAL GAMMA RAY SPECTRAL ANALYSIS

Sample	Disintegrations per Minute ($\times 10^{10}$)	
	Cs-137	Cs-134
1	7.45	4.55
2	11.1	6.72
3	13.6	7.58
4	10.0	6.59
5	13.2	6.99
6	16.7	8.33
7	20.0	11.51
8	22.6	10.42
9	18.2	11.03
10	<u>9.83</u>	<u>6.21</u>
Total	142.7 ^(a)	79.93 ^(b)
Specific loading (mg isotope/g carbon)	20.7	0.79

(a) $142.7 \text{ dpm} = 643 \text{ mCi} = 3.18 (\times 10^{19}) \text{ atoms} = 7.26 \text{ mg.}$

(b) $79.93 \text{ dpm} = 360 \text{ mCi} = 1.28 (\times 10^{18}) \text{ atoms} = 0.276 \text{ mg.}$

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6. FUEL ROD ENGINEERING (189a No. 00583)

The objective of this task is to evaluate the steady-state and transient performance of the fuel, blanket, and control rods for the determination of performance characteristics, operating limits, and design criteria. To this end, analytical tools [such as the LIFE-III code (Ref. 6-1)] are being adapted and/or developed and applied to analysis of GCFR prototypical and experimental rods. In addition, continuous surveillance of the LMFBR fuels and materials development program and technology is maintained to maximize the use of development technology and material properties. Support is also given to planning and designing of irradiation experiments.

6.1. FUEL, BLANKET, AND CONTROL ROD ANALYTICAL METHODS

6.1.1. Isotopic Fission Gas Release with Trapping and Resolution

Since the GCFR fuel rod is designed to be vented, it is important that the release of gaseous fission isotopes from the fuel be well predicted to establish the radioactivity in the venting lines and HPS. Previously, the spherical diffusional model of Booth was employed for fission gas release (Ref. 6-2). Based on this formulation, a computer subroutine called GAREL was written for incorporation in the fuel rod integral performance code LIFE and is being applied to the HELM-2 irradiation test (Ref. 6-3). From the standpoint of engineering design, the Booth model is simple and practical. It integrates the unaccounted for physical variables and mechanisms into the diffusion coefficient. Thus, the diffusion coefficient used in the calculation is an "apparent" and not a "true" diffusivity.

Among the important physical processes which have been neglected in the spherical diffusional model are trapping and resolution. It has been suggested (Refs. 6-4,6-5) and confirmed by electron microscope observation (Ref. 6-6) that in the absence of cracking, release proceeds by continuous accumulation of gas at grain boundaries until the growth at the grain boundaries becomes so large that the boundaries interlink. This implies that the grain boundaries behave as efficient sinks to trap gas atoms. Furthermore, it has been recognized for some time that gas atoms may become trapped at natural or irradiation-induced structural defects such as dislocation lines, impurities, and vacancy clusters. The presence of such traps will obviously modify the rate of transfer of gas atoms to grain boundary sinks to an extent depending on the trap concentration and the ability of the gas atoms to escape from traps after capture.

On the other hand, the trapped gas atoms in bubbles may be driven back into the matrix by irradiation. This process is called resolution. There are various explanations of the resolution phenomenon. Ross (Ref. 6-7) attributes resolution to the occurrence of a thermal spike in the vicinity of a gas bubble; Whaplam (Ref. 6-5) believes that fission fragments can blast off chunks of fuel from the bubbles' inner surface which carry gas atoms back to matrix; Turnbull (Ref. 6-8) proposes that a bubble is entirely destroyed (i.e., the gas atoms within it are redissolved) whenever a fission track intersects the bubble. All these resolution mechanisms have in common the feature of partial or complete destruction of a bubble by a single fission fragment. Therefore, the resolution phenomenon cannot be ignored in the fissioning fuel. The effect of trapping and resolution on fission gas release has been studied by many investigators (Refs. 6-9 through 6-11). A new formulation has been developed which closely follows Carroll's approach (Ref. 6-9), but to be consistent with Booth's model, is applied to a spherical particle rather than an infinite slab.

6.1.2. Release-to-Birth Rate

The relationship between release-to-birth rate (R/B) and fission gas release from the entire fuel rod is derived in Ref. 6-2. For convenience, the main equation (Eq. 30 of Ref. 6-2) is presented below.

$$R_{\text{fuel rod}} = 2\pi y_H \int_{-L/2}^{L/2} \dot{F} dz \int_0^R (R/B) r dr \quad ,$$

where L = length of fuel column,

R = pellet radius.

For the new formulation with the effects of trapping and resolution included, it has been found that the R/B is obtained by

$$\begin{aligned} R/B = \frac{R_H}{\frac{4}{3}\pi a^3 y_H \dot{F}} = \frac{3(\lambda_M^{WP} + y_H \dot{F})}{y_H \dot{F}} \left[\frac{1}{a\gamma_H} \coth\gamma_H a - \frac{1}{(a\gamma_H)^2} \right] \\ + \frac{3\lambda_M^{WP}}{y_H \dot{F}(\gamma_M^2 - \gamma_H^2)} \left[\frac{\gamma_H}{a} \coth\gamma_H a - \frac{\gamma_M}{a} \coth\gamma_M a \right] \quad , \end{aligned}$$

$$\gamma_H^2 = \frac{\lambda_H}{D_H} \left(1 + \frac{g}{\lambda_H + b} \right) \quad ,$$

$$\lambda_M^2 = \frac{\lambda_M}{D_M} \left(1 + \frac{g}{\lambda_M + b} \right) \quad ,$$

$$W = 1 + \frac{g}{\lambda_M + b} \left[r_o + (1 - r_o) \frac{b}{\lambda_H + b} \right] \quad ,$$

$$P_M = \frac{y_M \dot{F}}{D_M \gamma_M^2} = \frac{y_M \dot{F}}{\lambda_M (1 + \frac{g}{\lambda_M + b})} \quad ,$$

where M,H = mother and daughter isobars, respectively,

D_x = diffusion coefficient for isotope x (m^3/s),

λ_x = decay constant for isotope x (s^{-1}),

y_x = fission yield of isotope x (atoms/fission),

g = probability of trapping (s^{-1}),

b = probability of escape from traps (s^{-1}),

$\dot{F}$ = fission rate density (fissions/ m^3 -s),

r_o = fractional probability that trapped parent atom will escape trap when it decays into daughter isobars,

a = radius of fissioning fuel sphere.

The new model represents a step forward in improving the current operational isotopic fission gas release model by including trapping, resolution, and the effect of precursors. In view of the complicated behavior of the fission gas within the fuel matrix, this model is by no means the ultimate model for characterizing the whole spectrum of physical processes and variables of fission gas behavior. For instance, the intimate relationship between fuel swelling and fission gas release cannot be revealed by this model; it has to be assessed by bubble migration theory. However, inclusion of all physical processes and variables in the fission gas behavior model is almost beyond the reach of the available analytical tools and, above all, is impractical from the standpoint of the objective of determining the radioactive isotopic gas release for the GCFR vented fuel rod design. Thus, the model will be combined with the sweep gas irradiation tests (GB-9 and GB-10) and the HELM series to establish a reasonable theoretical and empirical tool for predicting radioactive isotopic fission gas release for the GCFR vented fuel rod design.

6.2. ANALYSIS OF IRRADIATION TESTS

Since the general objective of the F-1 experiment was to study fuel rod irradiation behavior in the EBR-II fast flux environment over a wider range of temperatures to a higher range of cladding temperatures than those carried out in the LMFBR program, it is important that the cladding

temperature history of each F-1 fuel rod be well characterized. Several analyses have been done, but none has systematically performed over the complete operating history of each F-1 rod. Consequently, a complete final thermal analysis of the F-1 test capsules has been undertaken using the two-dimensional heat transfer code TAC2D (Ref. 6-12). The physical model, material thermal properties, heat generation rate, and coolant conditions used in the analysis are summarized below.

6.2.1. Physical Model

The heat transfer problem for the test capsules in EBR-II was assumed to be axisymmetric. Figure 6-1 illustrates the R-Z plane of the capsule thermal model used in the TAC2D temperature calculations. It was assumed that all the heat generated in the capsule was removed by its own coolant and there was no heat loss to surrounding assemblies.

6.2.2. Thermal Properties

The thermal conductivities used in the code were as follows. For the blanket material (uranium oxide) (Ref. 6-13),

$$K_{UO_2} = \frac{41.26 \times 10^2 (1 - P)}{1 + 0.5P} \frac{1}{129.4 + T_K} + 1.60173 \times 10^{-1} T_K^3 \text{ W/m-}^\circ\text{C} ,$$

where P = fractional porosity,

T_K = temperature (K).

For the fuel material (mixed oxide) (Ref. 6-14),

$$K_{(U,P)O_2} = \frac{1.079 \times 10^2 (1 - P)}{1 + 0.5P + 4.52P^2} \frac{1}{0.06066 + 0.030421T_K} + 7.514 \times 10^{-11} T_K^3 \text{ W/m-K} .$$

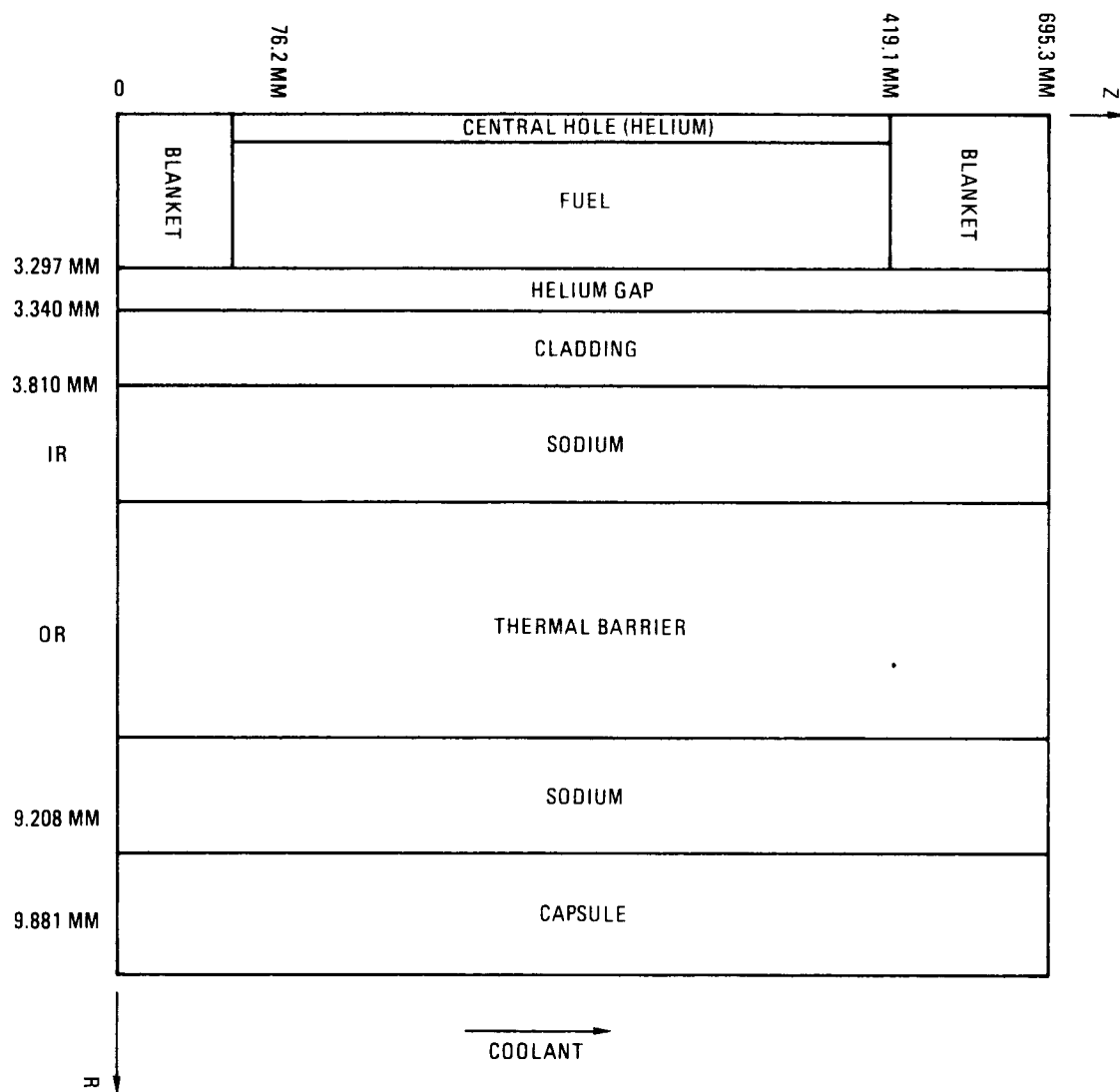


Fig. 6-1. Thermal model for F-1 test capsule

For the cladding material (316 stainless steel) (Ref. 6-14),

$$K_{316} = 0.1424 \times 10^2 + 0.1354 \times 10^{-1} T_c \text{ W/m-}^\circ\text{C} \quad ,$$

where T_c = temperature ($^\circ\text{C}$).

For the thermal barrier and capsule material (304 stainless steel) (Ref. 6-14),

$$K_{304} = 0.1426 \times 10^2 + 0.1343 \times 10^{-1} T_c + 0.1376 \times 10^{-6} T_c^2 \text{ W/m-}^\circ\text{C} \quad .$$

For sodium (Ref. 6-14),

$$K_{Na} = 0.929 \times 10^2 - 0.032 T_c + 0.117 \times 10^{-4} T_c^2 \text{ W/m-}^\circ\text{C} \quad .$$

For helium (Ref. 6-15),

$$K_{He} = 2.774 \times 10^{-3} T_K^{0.701} \text{ W/m-K} \quad .$$

6.2.3. Heat Generation Rate

The run-by-run peak linear powers of the fuel rods were obtained from the four-point fission rates provided by the EBR-II project using the LAMBDA code (Ref. 6-16); they are shown in Table 6-1 along with their curation. Figure 6-2 shows the idealized rod axial power profile obtained from a postirradiation gamma scan of rod G-4 (Ref. 6-17). This profile was assumed to be unchanged and applicable to the other fuel rods. The gamma heating of the capsule structural materials was also included (Ref. 6-18).

6.2.4. Coolant Conditions

The primary coolant flow rates used for the 7-rod assembly were 1.104×10^{-2} and $1.167 \times 10^{-2} \text{ m}^3/\text{s}$ prior to and after run 55, respectively. The coolant inlet temperature was assumed to be 371°C .

TABLE 6-1
F-1 ROD PEAK LINEAR POWER (KW/M)

Duration (hr)	Rod													
	Run No.	G-1	G-2	G-3	G-4	G-5	G-6	G-7	G-8	G-9	G-10	G-11	G-12	G-13
194	47B	39.0	41.3	42.0	42.3	44.6	44.3	45.9						
30	48	36.4	38.7	39.4	39.7	41.3	41.7	42.7						
251	49B	39.7	39.7	42.7	42.7	45.0	44.6	45.9						
116	49C	39.0	41.3	42.3	42.3	44.6	44.0	45.6						
331	50F	38.4	40.7	41.7	42.0	44.0	44.0	45.6						
210	50H	36.8	39.7	39.0	40.4	43.4	41.7	44.0						
268	51A	39.7	42.3	43.0	43.6	46.3	45.3	47.2						
349	51C	39.4	42.0	42.7	43.0	45.6	44.6	46.6						
162	52A	37.7	40.0	40.7	40.7	43.3	42.3	44.0						
435	52C	37.7	40.0	40.7	41.6	43.6	42.3	44.3						
80	53A	38.1	40.7	41.0	41.3	44.0	43.0	45.0						
140	53D	36.4	38.7	39.4	39.7	42.0	41.7	43.3						
376	53E	35.8	38.1	38.7	39.0	41.3	41.0	42.7						
584	54	36.4	38.4	39.4	39.4	41.7	41.0	42.7						
596	56	45.3	45.6		43.0	43.6	40.7	40.7	46.3					
548	57	43.6	44.3		41.3	42.0	39.4	39.4	44.6					
596	58	45.3	45.9		43.3	43.6	41.3	41.0	46.6					
596	59	44.6	45.3		43.0	42.0	40.4	40.7	45.6					
595	60	44.6	45.3		42.7	43.0	40.4	40.7	45.6					
595	61	45.0	45.6		42.7	43.3	40.7	40.7	45.9					
248	67A				43.0				46.9	45.6	47.2	49.2	45.3	49.5
281	67B				43.3				47.2	45.6	47.6	49.5	45.3	49.9
586	68				45.3				48.9	47.2	49.5	52.2	47.2	52.2
595	69				44.3				45.3	45.9	47.9	50.9	46.6	43.2
687	70				45.6				49.2	47.6	49.5	52.2	47.2	52.2
631	71				43.3				45.9	44.3	46.9	49.5	44.6	48.9
566	72				44.0				46.3	45.3	47.2	49.9	45.6	49.2
806	73				43.0				45.0	44.0	46.3	48.6	44.6	47.9
1046	74				39.4				43.0	42.0	43.3	45.3	41.3	45.6
1038	75				39.4				43.3	42.3	43.6	45.3	41.7	45.6
644	76				40.7				45.3	44.0	45.3	47.2	43.0	47.6
1509	77				39.4				43.3	42.0	43.3	45.3	41.3	45.6
1080	78				38.4				42.3	41.0	42.7	44.3	40.4	44.6
1079	79				39.4				43.6	42.7	43.6	45.3	41.7	45.6
992	80B				35.1				39.4	38.1	39.4	40.7	37.1	41.7
1117	81				37.1				41.7	40.4	41.3	43.0	39.0	43.6

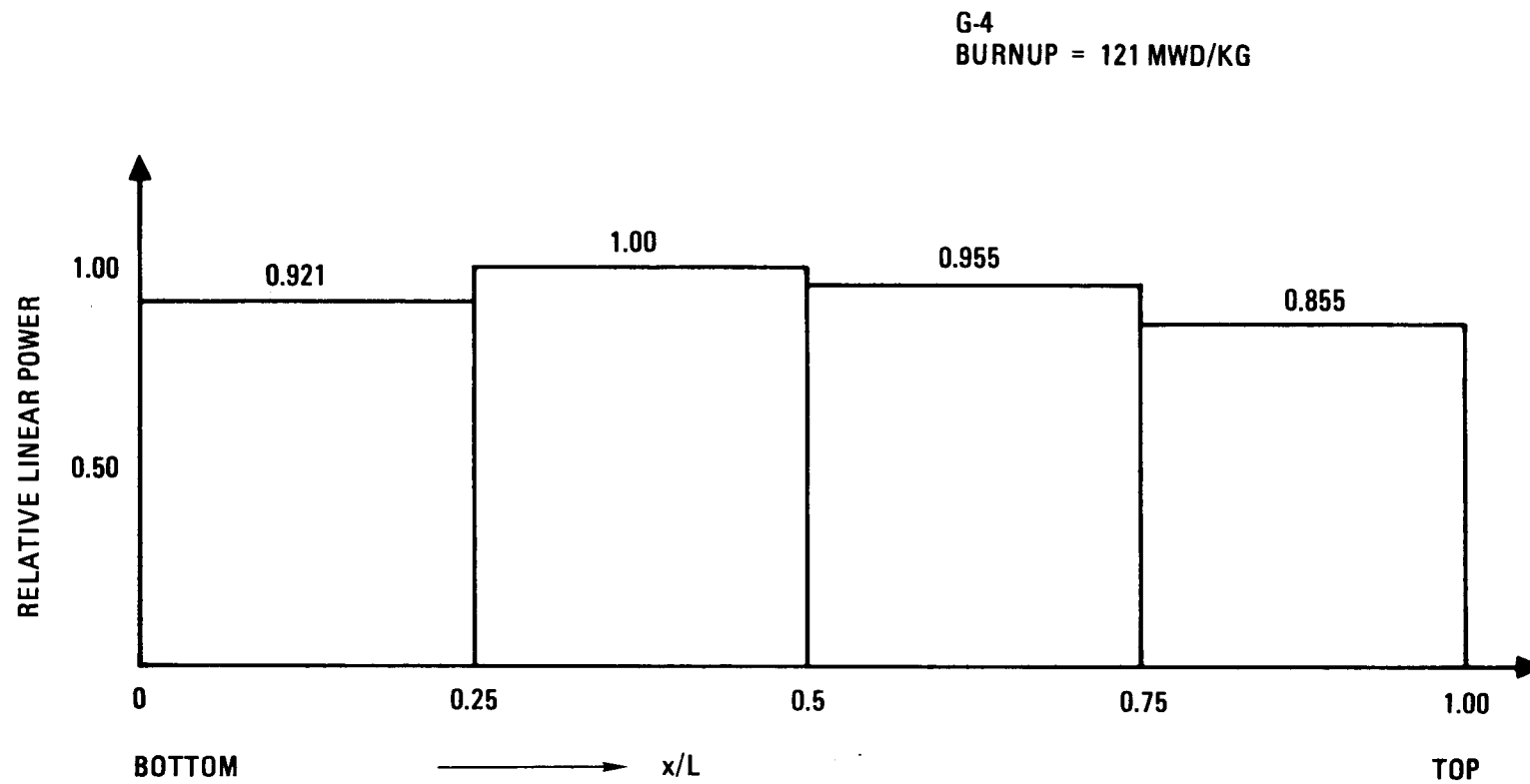


Fig. 6-2. F-1 fuel rod axial power profile

6.2.5. Results

The results of the run-by-run maximum cladding temperature experiment are given in Table 6-2 along with the liner powers. Figure 6-3 is a sample plot of the run-by-run peak cladding temperatures for rod G-1.

6.3. ROD ANALYSIS AND PERFORMANCE

A parametric study was performed for the maximum linear power of the boron carbide (B_4C) control rod based upon given maximum absorber temperatures and cladding o.d. temperatures. The analysis covered the variation of the maximum B_4C temperature from 800° to $2400^\circ C$ and the cladding o.d. temperature from 600° to $700^\circ C$. The results plotted in Fig. 6-4 show that maximum permissible control rod linear power varies almost linearly with maximum absorber temperature. In the present approach, linear power is independent of rod o.d., because the cladding thickness and absorber-to-cladding gap size are assumed to vary linearly with cladding o.d., and the gap conductance model is simplified. The proportional constants are taken from the fast flux test facility (FFTF) control rod design (Ref. 6-19).

The effects of thermal expansion, irradiation-induced swelling, and irradiation on thermal conductivity of B_4C were not included in the present analysis. Irradiation exposure causes significant decreases in the thermal conductivity of B_4C . The degradation is dependent on irradiation temperature and saturates rapidly at a burnup of 5×10^{20} captures/cm³ (Ref. 6-20). In operating temperature ranges, the thermal expansion coefficient of 316 stainless steel (Ref. 6-14) is greater than that of B_4C (Ref. 6-20). Therefore, the gap size tends to increase. Furthermore, ignoring the effect of surface roughness on the temperature jump distances causes an increase in gap conductance. In view of these nonconservative simplifications in the preliminary assessment of the maximum permissible linear power for the control rod and the LMFBR development program [the maximum linear powers are 31 and 14.8 KW/m for the Clinch River breeder reactor (CRBR) and FFTF, respectively (Ref. 6-21)], it is recommended that the maximum permissible linear power for the GCFR control rod be 30 KW/m.

TABLE 6-2
F-1 MAXIMUM CLADDING TEMPERATURES

Run No.	Rod													
	G-1		G-2		G-3		G-4		G-5		G-6		G-7	
	kW/m	°C	kW/m	°C	kW/m	°C	kW/m	°C	kW/m	°C	kW/m	°C	kW/m	°C
47B	39.0	699	41.3	697	42.0	662	42.3	674	44.6	705	44.3	660	45.9	640
48	36.4	681	38.7	679	39.4	646	39.7	657	41.3	683	41.7	644	42.7	622
49B	39.7	704	39.7	686	42.7	667	42.7	676	45.0	707	44.6	662	45.9	640
49C	39.0	699	41.3	697	42.3	664	42.3	674	44.6	705	44.0	658	45.6	639
50F	38.4	695	40.7	692	41.7	660	42.0	672	44.0	701	44.0	658	45.6	639
50H	36.8	683	39.7	686	39.0	644	40.4	661	43.3	696	41.7	644	44.0	629
51A	39.7	704	42.3	704	43.0	669	43.6	683	46.3	716	45.3	666	47.2	648
51C	39.4	702	42.0	702	42.7	667	43.0	678	45.6	712	44.6	662	46.6	644
52A	37.7	690	40.0	688	40.7	654	40.7	663	43.3	696	42.3	648	44.0	629
52C	37.7	690	40.0	688	40.7	654	41.6	665	43.6	699	42.3	648	44.3	631
53A	38.1	692	40.7	692	41.0	656	41.3	668	44.0	701	43.0	652	45.0	635
53D	36.4	681	38.7	679	39.4	646	39.7	657	42.0	687	41.7	644	43.3	626
53E	35.8	676	38.1	674	38.7	641	39.0	652	41.3	683	41.0	640	42.7	622
54	36.4	681	38.4	676	39.4	646	39.4	655	41.7	685	41.0	640	42.7	622
					G-8									
					kW/m	°C								
56	45.3	740	45.6	728	46.3	680	43.0	659	43.6	696	40.7	633	40.7	607
57	43.6	728	44.3	719	44.6	670	41.3	664	42.0	685	39.4	625	39.4	600
58	45.3	740	45.9	730	46.6	682	43.3	677	43.6	696	41.3	637	41.0	609
59	44.6	735	45.3	726	45.6	676	43.0	659	43.0	692	40.4	631	40.7	607
60	44.6	735	45.3	726	45.6	676	42.7	672	43.0	692	40.4	631	40.7	607
61	45.0	737	45.6	728	45.9	678	42.7	672	43.3	694	40.7	633	40.7	607
	G-9		G-10						G-11		G-12		G-13	
	kW/m	°C	kW/m	°C					kW/m	°C	kW/m	°C	kW/m	°C
67A	45.6	694	47.2	707	46.9	684	43.0	675	43.2	712	45.3	701	49.5	741
67B	45.6	694	47.6	709	47.2	686	43.3	677	49.5	714	45.3	701	49.9	744
68	47.2	704	49.5	722	48.9	696	45.3	690	52.2	731	47.2	714	52.2	759
69	45.3	696	47.9	711	45.3	674	44.3	683	50.9	723	46.6	709	49.2	739
70	47.6	706	49.5	722	49.2	698	45.6	692	52.2	731	47.2	714	52.2	759
71	44.3	685	46.5	705	45.9	678	43.3	677	49.5	714	44.6	696	48.9	737
72	45.3	691	47.2	707	46.3	680	44.0	681	49.9	717	45.6	703	49.2	739
73	44.0	683	46.3	700	45.0	672	43.0	675	48.6	708	44.6	696	47.9	731
74	42.0	670	43.3	681	43.0	659	39.4	651	45.3	688	41.3	674	45.6	715
75	42.3	672	43.6	683	43.3	661	39.4	651	46.3	688	41.7	676	45.6	715
76	44.0	683	45.3	694	45.3	674	40.7	660	47.2	700	43.0	685	47.6	728
77	42.0	670	43.3	681	43.3	661	39.4	651	45.3	688	41.3	674	45.6	715
78	41.0	663	42.7	677	42.3	655	38.4	645	44.3	681	40.4	668	44.6	709
79	42.7	674	43.6	683	43.6	664	39.4	651	45.3	688	41.7	676	45.6	715
80B	38.1	644	39.4	655	39.4	637	35.1	623	40.7	659	37.1	645	41.7	689
81	40.4	659	41.3	668	41.7	651	37.1	636	43.0	673	39.0	659	43.6	702

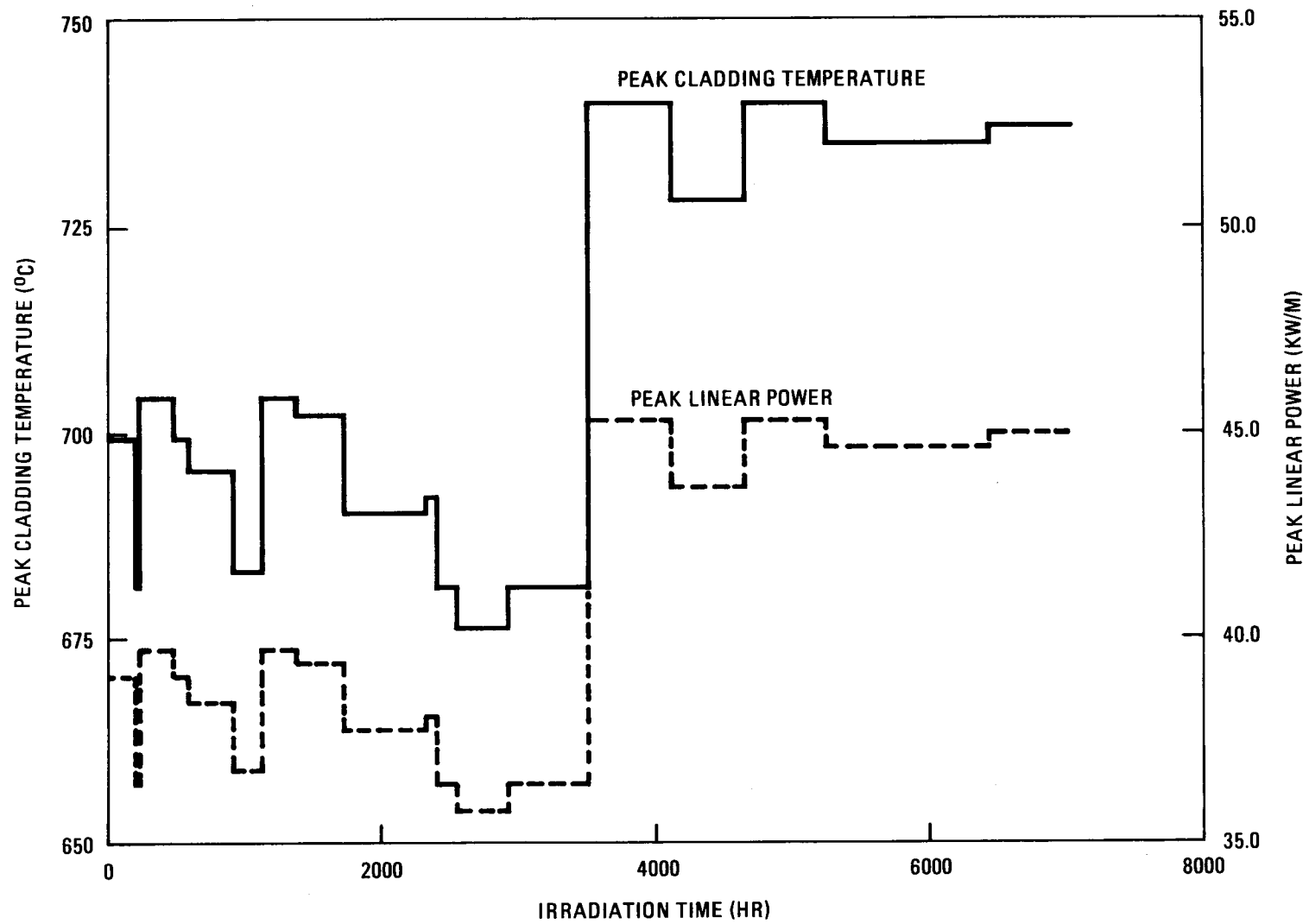


Fig. 6-3. F-1 G-1 peak cladding temperature and linear power

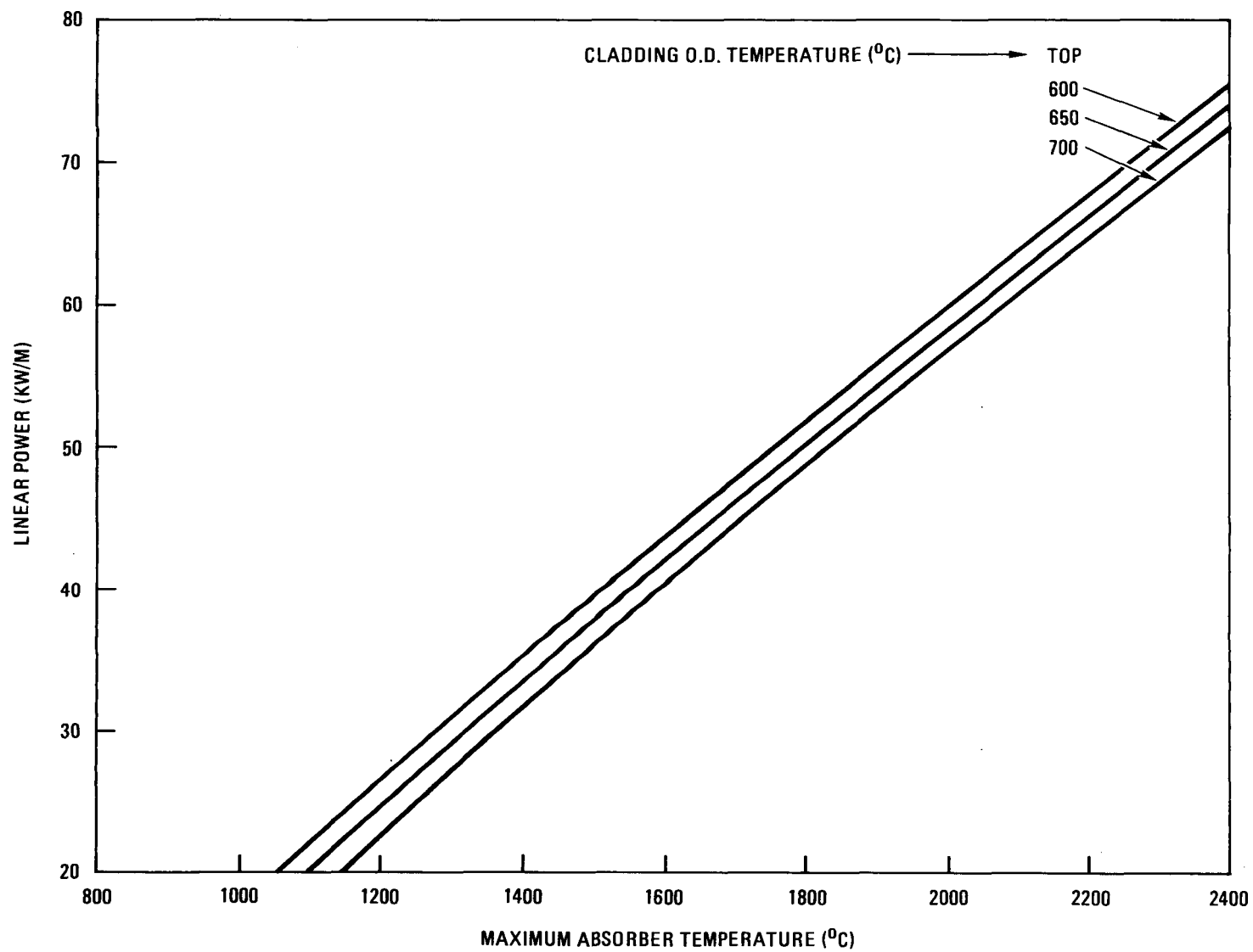


Fig. 6-4. Maximum control rod linear power

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7. NUCLEAR ANALYSIS AND REACTOR PHYSICS (189a No. 00584)

The scope of activities planned under this subtask encompasses validation and verification of the nuclear design methods which will be applied to the GCFR core design. This will primarily be done by evaluating the methods using a critical assembly experimental 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 previous quarter, calculations of the phase II GCFR critical assembly experiments neared completion. Included in those efforts were two-dimensional diffusion theory calculations in XY geometry with detailed modeling of the as-built phase II configurations on ZPR-9. Using these models, power profiles were derived for comparison with fission rate measurements using foil irradiations. Testing of the ENDF/B-4 data for U-233 was carried out using discrete ordinates calculations for the U-233 bare-sphere benchmark.

During the present quarter, detailed modeling of the XY midplane geometry continued with derivation of central worths and worth profiles. The spectrum code improvement resulted in a stand-alone version of the synthetic-kernel-method (SKM2) (Ref. 7-1) which will replace the GAROL (Ref. 7-2) resolved resonance code. The two-dimensional code 2DB (Ref. 7-3) has been issued as a production code with many improvements over earlier versions. Testing of ENDF/B-V was carried out for the U-233 bare-sphere "Jezebel" benchmark, with significant eigenvalue improvement.

7.1. GCFR CRITICAL ASSEMBLY ANALYSIS AND PLANNING

7.1.1. Diffusion Theory Calculations in X-Y Geometry for Phase II

The series of two-dimensional diffusion theory calculations begun during the last quarter was continued and expanded with the selection of four reference phase II configurations for definitive analysis. The cases chosen were

1. Dry, no reflector, critical.
2. Dry, reflected, critical.
3. Wet, reflected, critical.
4. Dry, reflected, subcritical (rodded).

Wet refers to the core filled with steam simulant in the voided channels of the matrix tubes.

The modified 2DB code (Ref. 7-4) was used in 10 and 28 energy groups to produce real and adjoint fluxes in XY geometry. The two-dimensional code PERT-V (Ref. 7-5) was then employed to calculate the perturbation denominator and the central worths for isotopic analysis. A comparison of the radial profiles with ANL foil measurements and experimental control worth values is in progress. Cell-homogenized cross sections were used to simulate the samples actually used in the experiments. Table 7-1 gives the calculated central worths in the two group structures for cases 1 through 3. There is reasonable consistency between the 10- and 28-group structures, with the expected exceptions for hydrogen. Little difference is evident between the bare and reflected dry worths, as expected from the lack of a leakage component of worth at the reactor center.

TABLE 7-1
PHASE II CENTRAL WORTHS, HOMOGENEOUS CROSS-SECTIONS

	Unreflected, Dry		Reflected, Dry		Reflected, Wet	
	Energy Group					
	28 (942.5 Ih/%ΔK/K)	10 (935.4 Ih/%ΔK/K)	28 (943.2 Ih/%ΔK/K)	10 (937.0 Ih/%ΔK/K)	28 (936.9 Ih/%ΔK/K)	10 (931.8 Ih/%ΔK/K)
H	-166.4	-1785	-138.7	-1773	+2073	+1253
He	-194.1	-194.6	-195.6	-196.4	-156.8	-155.8
Be	-52.97	-59.12	--	--	--	--
Li-6	-2881	-2857	-2394	-2876	-3533	-3457
B-10	-3481	-3383	-3500	-3408	-5406	-5184
C	-35.15	-37.24	-35.25	-37.46	-17.22	-18.56
O	-23.05	-22.75	-23.14	-22.88	-13.56	-13.32
Al	-12.83	-11.45	--	--	--	--
Si	-12.31	-15.09	--	--	--	--
Cr	-9.568	-9.028	-9.622	-9.090	-9.220	-8.813
Mn	-14.91	-17.93	-15.00	-18.08	-24.20	-33.25
Fe	-8.275	-8.325	-8.323	-8.377	-7.626	-7.390
Ni	-13.15	-14.02	-13.20	-14.09	-12.19	-12.68
Mo	-24.50	-23.80	-24.65	-23.98	-34.78	-33.46
Eu	-181.4	-171.4	--	--	--	--
Th	-25.35	-24.52	-25.50	-24.71	-40.61	-38.73
U-233	+338.0	+335.7	+339.6	+338.0	+367.3	+360.1
U-235	+197.5	+196.6	+198.5	+198.0	+206.6	+205.7
U-238	-12.37	-11.98	-12.45	-12.09	-15.21	-14.79
Pu-238	+171.7	+171.6	--	--	--	--
Pu-239	+269.7	+267.9	+270.8	+269.5	+266.7	+265.3
Pu-240	+49.18	+49.55	+49.35	+49.75	+28.54	+30.47
Pu-241	+358.0	+356.9	+361.1	+359.3	+398.9	+392.2
Pu-242	+39.91	+40.28	+40.05	+40.43	+10.22	+13.75
Am-241	+15.06	+17.35	+15.04	+17.29	-36.12	-29.48

7.1.2. Planning for Future GCFR Critical Programs

Four distinct experimental phases have been identified:

1. Two-zone core, (Pu/U) O_2 fuel, UO_2 radial blanket, and ThO_2 axial blanket sector.
2. Two-zone core, high Pu-240 central zone, and ThO_2 axial blanket sector.
3. Three-zone reference core with extensive control rod and power mapping studies.
4. Three-zone core, (Pu/Th) O_2 central zone, and UO_2 blankets.

These phases and the associated experiments are expected to complete the GCFR nuclear data base and to provide a considerable degree of data validation for the proliferation-resistant alternate fuel cycle designs being studied.

7.2. METHODS DEVELOPMENT

7.2.1. Spectrum Code Improvement

Updating of the synthetic kernel method portion of the MICROX code (Ref. 7-6) has continued, resulting in a stand-alone version of the SKM2 code. This code, which is intended to replace the exact elastic scattering kernel method in the spectrum calculation for the resolved energy region in the GAROL section of GGC-5 (Ref. 7-7), differs from the original SKM code by, among other things, inclusion of an additional delta function in the P_0 synthetic scattering kernels. The additional term allows preservation of one more moment of the exact elastic scattering kernel with very little extra computational effort. P_1 synthetic scattering kernels have also been added to make a true B_1 calculation possible in a

two-region problem. This new B_1 treatment allows direct computation of self-shielded broad-group P_1 transfer arrays from hyperfine mesh currents and cross sections, in the same manner as the P_0 transfer array computations in the MICROX code. As before, the two regions of the unit cell are coupled via P_0 collision probabilities. In the new algorithms, the effects of anisotropic scatter on the collision probabilities are approximated by replacing the total cross section with the transport cross section during evaluation of the collision probabilities.

The direct generation of consistent P_0 and P_1 broad-group transfer matrices from the hyperfine mesh data allowed the solution of two persistent problems related to preparation of broad-group transport cross sections (i.e., diffusion coefficients). The theory behind the B_1 equation derivations yields a unique definition of the macroscopic transport cross sections in terms of the regionwise fluxes, currents, and input buckling. The assignment of this macroscopic transport cross section to the individual nuclides in the problem has, however, always been rather arbitrary and has often given anomalous and sometimes negative results for light nuclides in hard-spectrum systems.

A new formula that yields physically plausible regionwise microscopic transport cross sections as well as the "correct" macroscopic transport cross section per the B_1 equation definitions has been developed by combining definitions 1 and 2 of Ref. 7-8. Microscopic transport cross sections computed according to definition 2 (current weighted $\sigma_t - \bar{u}\sigma_s$) are now normalized to the B_1 equation macroscopic transport cross section $[B\Phi/(3J)]$.

The recurrent problem of how to prepare a diffusion coefficient (i.e., macroscopic transport cross section) for a two-region cell containing a low-density region, such as a channel containing gaseous coolant, was solved by using the Benoist equation (see Eq. 1 of Ref. 7-9) to prepare cell-averaged diffusion coefficients directly from the hyperfine mesh data. When the Benoist equation is averaged over all directions, the required collision probabilities turn out to be the multiple collision probabilities already used in the SKM and MICROX codes.

Table 7-2 compares the GAROL method for computing fine-group U-238 capture cross sections in the resolved resonance region with the new SKM2 code. A homogeneous model of the ZPR6-7 critical assembly which has been used for an interlaboratory spectrum code testing effort was recalculated with the SKM2 methodology and compared with the current production version of GAROL. As shown, the synthetic kernel method gives results which are generally comparable to GAROL (and much faster in computation time). The differences around fine-group 89 remain to be satisfactorily resolved.

7.2.2. Benchmark Test of ENDF/B-V Data for U-233

The criticality calculation for the U-233 Jezebel spherical assembly reported in Ref. 7-10 has been repeated with identical methods and a preliminary issue of version 5 nuclear data for U-233. The eigenvalue for the critical configuration rose about 2%, which is a consequence of the increased fission cross section. Table 7-3 illustrates the changes in integrated quantities.

TABLE 7-3
JEZEBEL PARAMETERS

	ENDF/B-IV	ENDF/B-V	Δ
k_{eff}	0.96158	0.98213	0.02055
Absorption	0.37905	0.38252	0.00347
Leakage	0.62234	0.61842	-0.00392

7.2.3. Production Version of 2DB Diffusion Code

The 2DB code is in extensive use throughout the fast breeder reactor community and has been adopted at GA as the production two-dimensional diffusion code for GCFR physics work. Numerous changes have been made to the original version to optimize the code for specific GCFR usage and for the current UNIVAC 1110 computer. These changes include

TABLE 7-2
U-238 CAPTURE CROSS SECTIONS FOR A HOMOGENEOUS
MODEL OF THE ZPR-6-7 CRITICAL ASSEMBLY

Fine Group No.	Lower Energy (eV)	GAROL	SKM2	Difference (%)
61	5531.1	0.8730	0.8728	0.02
62	4307.6	0.7803	0.7794	0.12
63	3354.7	0.8773	0.8768	0.06
64	2612.3	1.1946	1.1193	0.13
65	2034.6	1.0600	1.0585	0.14
66	1584.7	0.9958	0.9894	0.65
67	1234.3	0.9573	0.9559	0.15
68	961.1	1.1785	1.1734	0.43
69	748.5	1.2959	1.2904	0.43
70	582.9	1.3146	1.3083	0.48
71	453.9	1.2402	1.2322	0.65
72	353.6	1.1527	1.1474	0.46
73	275.3	1.1557	1.1507	0.43
74	214.4	1.5386	1.5370	0.10
75	167.0	2.2699	2.2752	-0.23
76	130.1	1.3777	1.3742	0.25
77	101.3	3.6315	3.5740	1.61
78	78.94	1.8867	1.8869	-0.01
79	61.47	2.9801	2.9247	1.89
80	47.86	0.1491	0.1486	0.34
81	37.30	1.6303	1.6187	0.72
82	29.03	12.701	11.966	6.14
83	22.61	0.6507	0.6564	-0.88
84	17.60	19.639	19.145	2.58
85	13.72	0.5027	0.4904	2.51
86	10.68	0.2870	0.2884	-0.49
87	8.298	0.8821	0.8866	-0.51
88	6.465	14.433	20.458	-41.7
89	5.052	10.832	7.3618	47.1
90	3.934	1.1683	1.1216	14.16
91	3.062	0.6629	0.6558	1.08
92	2.374	0.5278	0.5261	0.32

1. Optional bidirectional diffusion coefficient modifier input.
2. High-speed input/output using UNIVAC NTRAN routines.
3. Optional internal calculation of optimal group-dependent inner iteration overrelaxation factors, as in PDQ-5 (Ref. 7-11).
4. Second-order Chebyshev extrapolation of group fluxes (rather than fission sources) with internal calculation and periodic updating of the estimated dominance ratio for the outer iteration problem, as in PDQ-5.
5. Upscatter capability with very little loss of speed for downscatter problems.
6. A more general depletion calculation using internally computed, variable, small time step sizes depending upon the depletion rates actually encountered.
7. Separation of regular and adjoint flux input and output files to allow "exact" perturbation calculations. The structure of the flux files was changed to allow for addition of interactive plotting routines.

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8. SHIELDING REQUIREMENTS (189a No. 00584)

The purposes of the shielding task are to verify the adequacy of the methods and data (physics and engineering) for the design of GCFR shields and to evaluate the effectiveness of various shield configurations. This task also coordinates and provides liaison with the analytical and experimental shielding program at ORNL.

During the last quarter the shielding aspects of the two alternate 300-MW(e) GCFR designs were analyzed, and a response to ORNL specifications for the grid plate shielding design confirmation experiment was prepared. In addition, shutdown gamma ray, beta ray, and neutron source terms were evaluated for use in fuel handling equipment studies, and an investigation of the sensitivity of the radial shielding response to radial source distribution of radial zoning was completed. During this quarter, physics input for the material exposure service environment was provided for an assessment of material irradiation effect data requirements for out-of-core GCFR structural components. An EOEC RZ core and blanket model was determined for use in shielding analysis. Several design alternatives for lower plenum shielding were examined, and a study of methods to reduce lower plenum streaming was initiated. The DOT 3.5 code (Ref. 8-1) was made operational.

8.1. ASSESSMENT OF MATERIAL IRRADIATION EFFECT DATA REQUIREMENTS FOR OUT-OF-CORE STRUCTURAL COMPONENTS

A major effort during this quarter was providing input for an assessment of irradiation effect data requirements for GCFR structural components located outside the active core and blankets (Ref. 8-2). Such a data base is required for determining and/or validating component exposure

limits which determine the component shielding requirements. The assessment provides a priority list of additional irradiation tests and environments which will be required for GCFR design or design verification, but which are not included in the current national program on the effects of irradiation on the mechanical properties of fast breeder reactor structural materials.

As shown in Table 8-1, important GCFR structural components can be categorized according to their location in the reactor and the severity of the operating environment. These locations, in order of increasing neutron fluence and temperature, include the primary coolant system boundary, the upper reactor plenum, the core support system, and the lower reactor plenum. The highest-priority data need was determined to be additional high-temperature irradiation effect data to assess helium damage effects in lower-plenum GCFR components. Second priority was given to additional data at a higher fluence to increase fluence limits for the core support system.

8.2. SHIELDING CORE MODEL A

Sensitivity analyses (Ref. 8-3) have shown that out-of-core response values (e.g., grid plate or PCRV liner damage) are highly sensitive to core and blanket power distributions. In general, the sensitivity of response values to power distribution increases with increasing penetration to the response location of interest; source points located nearest the point at which the response is evaluated are most important in contributing to the response. The axial or radial blankets of a GCFR (or any fast reactor) complicate the procedure of obtaining an accurate source distribution, since fertile-to-fissile transmutation can result in large changes in blanket power distribution (and out-of-core response values) from year to year.

Reference 8-3 provides the basis for evaluating a two-dimensional (RZ) core and blanket model, model A, for use in shielding analyses at

TABLE 8-1
TEMPERATURES AND RADIATION EXPOSURE ENVIRONMENT CATEGORIES FOR GCFR STRUCTURAL COMPONENTS

Category	Applicable Components	Metal Temperature (°C)	Radiation ^(a) (φt)
Primary coolant system boundary	PCRv liner	Very low: ~70	Very low: ~10 ¹⁹
Upper reactor plenum	Reactor cavity closure shield and attachments Return duct shielding and attachments Fuel unlocking mechanism penetration ports Upper axial shield and attachments Thermocouple instrument trees	Low: 325 to 425	Low: <5 x 10 ²¹
Core support system	Reactor grid plate Core support cylinder	Low: 325 to 425	Moderate: ~10 ²²
Lower reactor plenum	Inner radial shield assemblies and attachments Outer radial shield segments and attachments Lower axial wraparound shield Lower axial preshield	High: 550 to 650	High: ≤2 x 10 ²³

(a) Total neutron fluence over plant life (actual exposures are much less for removable components in the lower plenum).

GA and ORNL. Until the model is revised to reflect changes in core design or fuel management strategies, it will provide the reference core and blanket source and spatial envelope for GA and ORNL shielding analyses. The model simulates a core with the following general characteristics:

1. Pressure drop = 0.290 MPa.
2. Number of fuel assemblies = 127.
3. Number of blanket assemblies = 90 (2 rows of uranium radial blanket assemblies)

Table 8-2 gives the core and blanket assembly major design parameters, and Fig. 8-1 depicts the radial blanket management scheme on which the model is based. Note that all radial blanket row 1 assemblies are rotated 180 deg after 1 yr of residence and are shuffled to row 2 without rotation at the end of 2 yr of residence, where they remain for two additional years. Six of the initially fresh row 2 assemblies remain in row 2 for 6 yr without rotation and are then replaced with fresh assemblies. Model A provides a power distribution corresponding to the end of the sixth fuel cycle (worst case), with no allowance for azimuthal power variation.

The RZ model and homogeneous material region designations and geometry are given in Fig. 8-2. Note that there are 3, 2, and 8 regions in the core, axial blanket, and radial blanket, respectively. Table 8-3 gives the uranium and plutonium isotope densities for each material region, and Table 8-4 gives the nuclide densities for steel, helium, and oxygen. An EOE eigenvalue of 1.0060 was obtained for this model using the 2DB diffusion code (Ref. 8-4).

For the purpose of obtaining a distributed fission source for RZ model A calculations, the full region detail of Fig. 8-2 should be retained in either a diffusion or transport eigenvalue calculation. Subsequent distributed source calculations using the fission source based on the detailed model may then be performed with homogenized core and blanket regions; system leakage is negligibly affected by small variations in uranium and plutonium distributions.

TABLE 8-2
300-MW(e) GCFR CORE ASSEMBLY MAJOR DESIGN PARAMETERS
FOR A 127-ASSEMBLY, 0.290-MPA CORE

Fuel assembly	
Cross section	Hexagonal
Length (mm)	3665.0
Active core height (mm)	1130.0
Number of fuel rods	264
Number of hanger rods	6
Number of instrumentation rods	1
Fuel rod spacer type	Modified hexagonal grid
Number of spacer grids	10
Across-flats i.d. (mm)	169.0
Across-flats o.d. (mm)	176.6
Duct wall thickness (mm)	3.8
Fuel rod pitch (mm)	10.18
Rod-to-rod gap (mm)	2.72
Rod-to-duct gap (mm)	1.42
Rod-to-duct gap (% of rod-to-rod gap)	52.0
Fuel assembly pitch (mm)	183.1
Fuel rod	
Length (mm)	2230.0
O.D. (mm)	7.46
Outside root diameter (mm)	7.20
Average volumetric diameter in rough section (mm)	7.275
I.D. (mm)	6.44
Cladding root thickness (mm)	0.38
Cladding material	20% cold-worked 316 stainless steel
Fuel and blanket pellet o.d. (mm)	6.30
Fuel and blanket pellet-to-cladding diametral gap (mm)	0.14
Fuel length (mm)	1130.0
Fuel material	Mixed $\text{PuO}_2\text{-UO}_2$
Fuel material planar smear density (% of theoretical density)	85.5
Axial blanket length (each) (mm)	450.0
Axial blanket material	Depleted UO_2
Axial blanket material planar smear density (% of theoretical density)	90.0
Blanket assembly	
Number of blanket rods	60
Number of instrumentation rods	1
Blanket rod size	
O.D. (mm)	19.65
I.D. (mm)	18.65
Cladding material	20% cold-worked 316 stainless steel
Blanket length (mm)	2030.0
Blanket material	UO_2
Blanket material smear density (% of theoretical density)	90.0
Lattice spacing (mm)	21.05
Blanket assembly spacer system	Wire wrap
Wire wrap diameter (mm)	1.4
Wire wrap pitch (mm)	300.0

FEVER TIME POINT CYCLE RELOAD			BLANKET ASSEMBLY RESIDENCE HISTORY													TIME POINT DESIGNATION	
			ROW 1			ROW 2											
			0	1	2	0	0	0	0	0	0	0	1	2	2	2	
			0	0	0	0	1	2	3	4	5	6	0	0	1	2	
0	1	1	42			48											BOL
1	1			42			48										EOC1
2	2	2		42*			48										BOC2
3	2				42			48									EOC2
4	3	3	42					6					42**				BOC3
5	3			42				6						42			EOC3
6	4	4		42*				6						42			BOC4
7	4				42				6						42		EOC4
8	5	5	42					6					42**				BOC5
9	5			42					6					42			EOC5
10	6	6		42*					6					42			BOC6
11	6				42					6					42		EOC6
12	7	7	42			6							42**				BOC7
13	7			42			6							42			EOC7
14	8			42*			6							42			BOC8
15	8				42			6							42		EOC8

*ALL ROW 1 ASSEMBLIES ROTATED 180 DEG

**ALL ROW 1 ASSEMBLIES SHUFFLED TO ROW 2

Fig. 8-1. Radial blanket management scheme

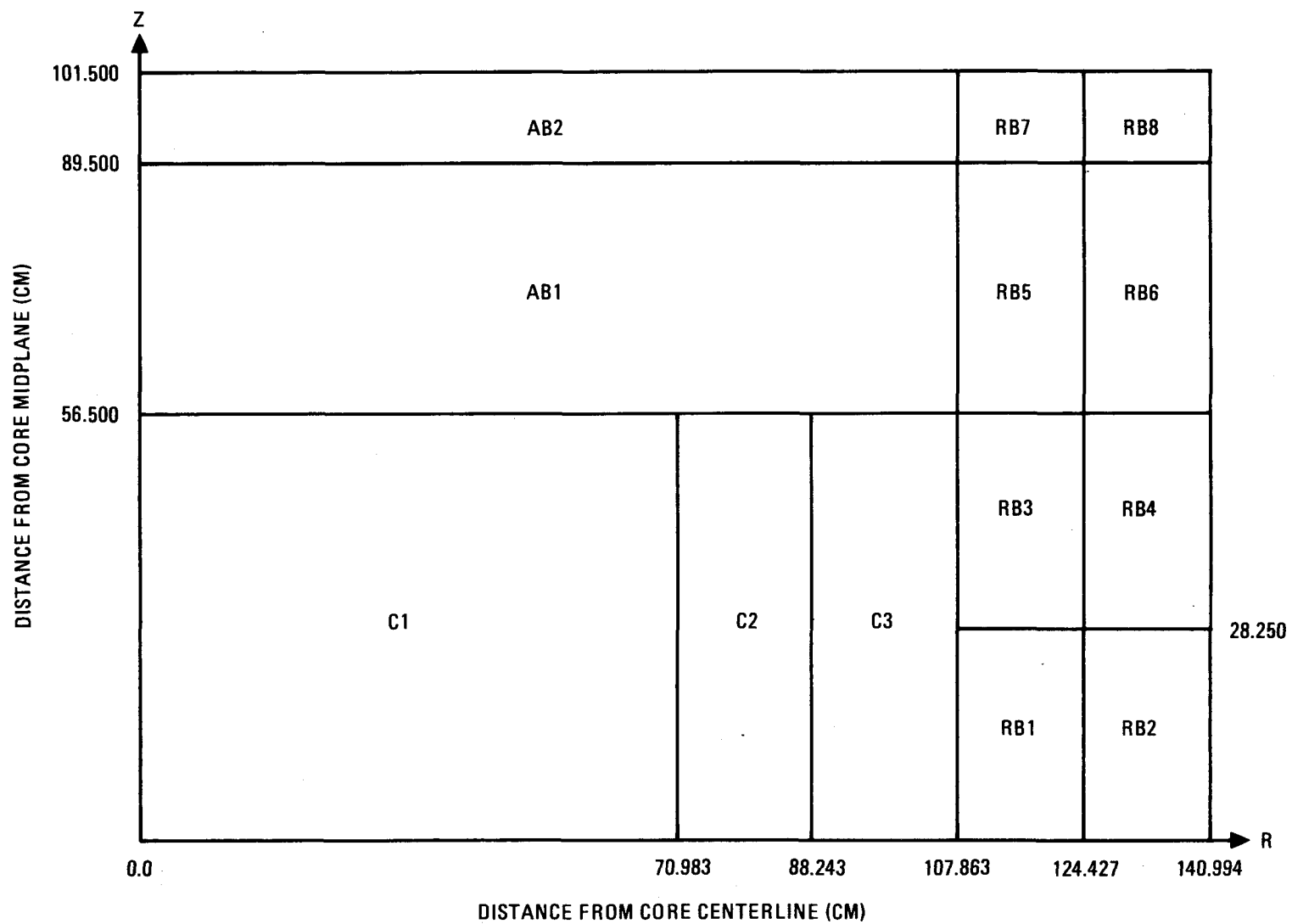


Fig. 8-2. Geometry and region designations for shielding core model A

TABLE 8-3
NUCLIDE DENSITIES FOR URANIUM AND PLUTONIUM ISOTOPES IN
0.290-MPA CORE AND BLANKETS

Region	Region Designation ^(a)	Nuclide Density (atoms/barn ⁻¹ -cm ⁻¹)					
		U-235	U-238	Pu-239	Pu-240	Pu-241	Pu-242
Core	C-1	8.2417-6 ^(b)	4.3849-3	5.7184-4	2.7327-4	9.1144-5	7.5379-5
	C-2	7.8966-6	3.9878-3	7.0124-4	3.4393-4	1.2273-4	9.6011-5
	C-3	8.4185-6	4.0179-3	8.8127-4	4.4226-4	1.6333-4	1.2206-4
Radial blanket	RB1	2.7005-5	1.2105-2	1.8185-4	3.1310-6	N ^(c)	N
	RB2	2.5082-5	1.2022-2	2.5873-4	9.1355-6	N	N
	RB3	2.8158-5	1.2161-2	1.2663-4	2.1803-6	N	N
	RB4	2.6819-5	1.2103-2	1.8017-4	6.3615-6	N	N
	RB5	2.9974-5	1.2250-2	3.9640-5	6.8251-7	N	N
	RB6	2.9555-5	1.2232-2	5.6398-5	1.9914-6	N	N
	RB7	3.0560-5	1.2278-2	1.1568-5	1.9918-7	N	N
	RB8	3.0438-5	1.2273-2	1.6459-5	5.8114-7	N	N
Axial blanket	AB1	1.3737-5	6.2176-3	7.5535-5	1.5902-6	N	N
	AB2	1.4731-5	6.2546-3	3.9642-5	4.5730-7	N	N

(a) See Fig. 8-2.

(b) 8.2417-6 = 8.2417 x 10⁻⁶.

(c) Negligible.

TABLE 8-4
NUCLIDE DENSITIES OF OTHER CONSTITUENTS IN
CORE AND BLANKETS OF 0.290-MPA CORE^(a)

Nuclide	Nuclide Density (atoms/barn ⁻¹ cm ⁻¹)		
	Core	Axial Blanket	Radial Blanket
He	4.4831-4 ^(b)	4.4831-4	2.4900-4
O	1.1989-2	1.2621-2	2.4642-2
Cr	2.8998-3	2.8998-3	2.2660-3
Fe	9.9817-3	1.9817-3	7.8001-3
Ni	1.9812-3	1.9812-3	1.5482-3
Mo	2.2452-4	2.2452-4	1.7545-4
Mn	2.8229-4	2.8229-4	2.2059-3

(a) From Ref. 8-3.

(b) 4.4831-4 = 4.4831×10^{-4} .

8.3. RADIAL SHIELD DESIGN ALTERNATIVES

8.3.1. Introduction

This section describes design alternatives which are being studied in an attempt to improve the radial shield design. The most viable combination of these alternatives will be selected as the reference shield and will be the basis for defining a radial shield experiment to be conducted in FY-80. The selection of the most viable alternatives will be based upon licensing, plant cost, and system interface considerations as well as shielding effectiveness.

8.3.2. Exposure Criteria

The important structures in the lower reactor plenum are the radial and lower axial shielding components, which protect the primary coolant system boundary (PCRVC concrete, liner, and tendons) and the PAFC. The material service conditions in the lower reactor plenum are, in general, severe in terms of fluence and temperature. The 30-yr fluence exposures range from 10^{21} to 2.0×10^{23} , with the maximum value occurring at the core midplane level adjacent to the radial blanket (actual fluence exposures for removable or nonremovable components are much less than 2×10^{23}). The high nuclear heating rates and cooling with 550°C core outlet helium results in high component temperatures (560° to 650°C for steel and 570° to 900°C for graphite). Of particular concern for the lower plenum components is the potential for significant helium damage effects such as

1. Low ductility and high swelling due to growth and coalescence of helium on grain boundaries.
2. Synergistic effects of helium and displacement damage which enhance void formation and swelling.

A combination of high flux and soft spectrum results in the possibility of significant helium generation rates due to (n,α) reactions in steel

trace impurities and the major alloying elements. The B-10 (n, α) and Ni-58 (n, δ) Ni-59 (n, α) Fe-56 reactions are the primary contributors, with large cross sections in the thermal and epithermal energy range. A small amount of existing irradiation data suggests that a few parts per million (atomic) of helium in stainless steel can seriously degrade mechanical properties at temperatures near and exceeding about one-half the absolute melting temperature. Lower plenum steel components operate in the temperature range where helium damage effects could dominate.

A review of the national program on the effects of irradiation on the mechanical properties of fast breeder reactor structural materials (Section 8.1) revealed that a conservative approach to defining GCFR component radiation exposure limits is required. This is because an adequate irradiation effect data base for defining component fluence limits (especially for lower plenum components) is not currently available, and much of the needed data will not be available until the mid-1980s. Moreover, sufficient data for validating energy-dependent damage correlations such as effective damage functions will not be available for several years. The GCFR demonstration plant project schedule may necessitate the use of the conservative exposure criteria used for the CRBR. Therefore, the following criteria have been proposed for evaluation of the revised reference shielding design:

1. Fluence limits for permanent components operating in the temperature range where helium damage is unimportant shall be based on total fluence. This neglects the energy dependence of neutron damage and is considered conservative, since no credit is taken for the fact that the service spectrum for out-of-core components is less damaging than the spectrum from which the total fluence data were obtained (primarily EBR-II core irradiations).
2. For components operating in the temperature range where helium damage may dominate, the total fluence shall not exceed 10^{21} so that only small concentrations of helium are produced.

3. The general exposure criterion for permanent steel components is a required residual EOL ductility of 10% based on total elongation.

8.3.3. Design Alternatives

Specific design alternatives for achieving the exposure criteria defined above have been identified. These alternatives

1. Assure a lifetime of at least 3 yr for the removable inner radial shield assemblies.
2. Eliminate the problem of streaming under the outer radial shield at the primary coolant system boundary streaming path position.
3. Incorporate design flexibility in anticipation of data allowing increased exposure limits.
4. Increase the amount of removable radial shielding.

Figure 8-3 shows the alternative concepts according to number. The alternatives and their purposes, advantages, and disadvantages are described below.

Alternative 1: one row of removable inner radial shield or blanket assemblies suspended on the grid plate and cooled by core inlet helium (same outside dimensions as the core and blanket assemblies).

Purpose: to assure low exposure levels to the permanent outer radial shield adjacent to the core and to increase the service life and reduce temperatures in the additional rows of inner radial shielding which are suspended on the outer radial shield.

Advantages: (1) Cost-effective design flexibility and added margin.
(2) A third row of blanket assemblies is an effective shield and

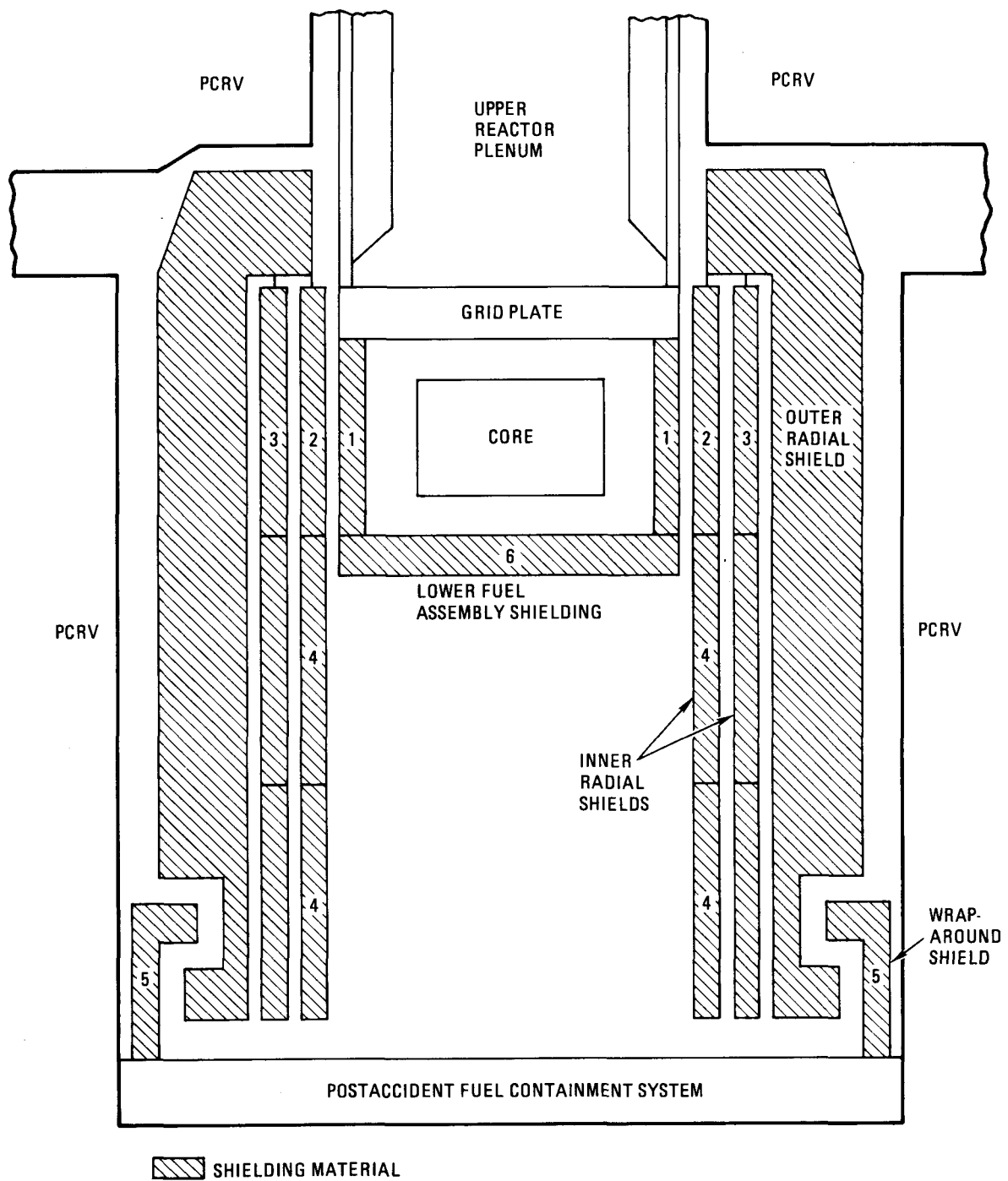


Fig. 8-3. Shielding design alternatives

also prototypical of a commercial plant. (3) A solid stainless steel assembly (with appropriate coolant passages) would provide an ideal reflector. (4) Low temperatures in the third row with inlet gas cooling would eliminate or significantly reduce helium damage effects. (5) Less stringent structural criteria would be applicable since the assemblies could be considered core assemblies (e.g., 1% ductility). (6) Additional benefit of added shielding for the grid plate to core support cylinder weld. (7) No additional requirements for fuel handling equipment. (8) Easy removal for testing and surveillance. (9) Possibly cheaper to fabricate than current inner radial shield concepts.

Disadvantages: (1) Increase in grid plate diameter and loading. (2) Possible increase in core cavity, PCRV, and containment diameter.

Alternative 2: a second row of removable radial shield assemblies suspended on the outer radial shield and cooled with core outlet helium.

Purpose: to assure low exposure levels to the permanent outer radial shield and provide additional attenuation of radiation to the PCRV at axial levels below the bottom of the core.

Advantages: (1) Added margin at the high-flux region adjacent to the core midplane, complementing the option of including a third row of radial blanket assemblies. (2) Low heating rates in a third row of removable radial shielding.

Disadvantage: possible additional functional requirements for fuel handling equipment.

Alternative 3: a third row of axially segmented radial shield assemblies suspended on the outer radial shield, cooled with core outlet helium, and extended to the bottom of the outer radial shield.

Purpose: to assure low exposure levels to the permanent outer radial shield and provide additional attenuation of radiation to the PCRV at axial levels below the bottom of the core.

Advantages: (1) Reduction in outer radial shield thickness and weight and support requirements. (2) Larger amount of the radial shielding is removable.

Disadvantages: (1) Possible difficulties in providing cooling, especially to the lower axial segments. (2) Additional functional requirements for fuel handling equipment.

Alternative 4: extension of the second and third rows of the inner shielding to the bottom of the outer radial shield.

Purpose: same as alternative 3 should one row of axial segmented shielding prove inadequate.

Advantages and disadvantages: same as alternative 3.

Alternative 5: incorporation of a multibend lower wraparound shield which mates with the bottom of the outer radial shield.

Purpose: reduction of flux at the PCRV streaming path position and the PCRV at axial levels below the bottom of the core.

Advantage: apparent easy mechanical solution to streaming under outer radial shield.

Disadvantages: additional primary system pressure drop. (2)
Possible incompatibility with PAFC system.

Alternative 6: shielding in lower portion of fuel assemblies, extension of lower axial blanket or both.

Purpose: reduction of axial leakage source to lower plenum to (1) reduce exposure to the outer radial shield; (2) reduce exposure to the PAFC system and preshield; and (3) reduce exposure to the PCRV at the streaming path position and axial levels below the bottom of the core. Thus, alternative 6 potentially accomplishes the combined purposes of alternatives 3, 4, and 5.

Advantages: (1) Elimination or reduction of requirements for axially segmented removable radial shielding and a multibend wraparound shield. (2) Possibility of removing the lower axial assembly shield segments with fuel handling equipment with no increase in PCRV or containment height.

Disadvantages: (1) If the shielding were integral to the fuel assemblies, an increase in core cavity, PCRV, and containment height would result. (2) Additional fuel assembly pressure drop. (3) Conflicting requirements for helium flow and shielding could result in a considerable increase in assembly length owing to streaming. (4) Detrimental effect on breeding ratio if highly absorbing shielding material is used to reduce shield volume and pressure drop.

Alternative 7: boronation of the removable radial shielding attached to the outer shield.

Purpose: to remove the low-energy flux component causing helium generation in the stainless steel.

Disadvantages: (1) Swelling and reduction in thermal conductivity of boron carbide. (2) High heating rates due to the B-10 (n, α) reaction. (3) Detrimental effect on breeding ratio.

Table 8-5 presents the possible design alternative combinations which could result in a revised design for lower plenum shielding which would

TABLE 8-5
DESIGN ALTERNATIVE COMBINATIONS FOR GCFR RADIAL SHIELDING DESIGN

Alternative	Combination						
	1	2	3	4	5	6	7
1. One row of radial shield or blanket assemblies	X	X	X	X	X	X	X
2. Second row of radial shield assemblies		X	X	X	X	X	X
3. Third row of axially segmented radial shield assemblies	X	X	X		X	X	
4. Axial segmentation of second row of radial shield assemblies			X				
5. Multibend lower axial wraparound shield	X	X	X			X	X
6. Lower fuel assembly shielding and/or blanket extension				X	X	X	X
7. Boronation of inner radial shield rows two and/or three	X	X	X	X	X	X	X

satisfy current criteria. It is anticipated that the use of boronated graphite in high-flux regions will be required to decrease the amount of helium damage.

8.4. 300-MW(e) GCFR LOWER PLENUM STREAMING

A study of a method for reducing the neutron streaming in the lower plenum of the 300-MW(e) GCFR was initiated. The method involves placement of B_4C near the interior radius of the outer radial shield for its entire length below the lower axial blanket. Figure 8-4 shows (1) the total neutron fluences of the reference 300-MW(e) GCFR; (2) the dominant direction of downward neutron streaming in the interior of the outer radial shield and under the outer radial shield; and (3) the dominant direction of upward streaming at the wraparound shield and between the outer radial shield and the PCRV liner. If the inner radial shield is extended downward the same length as the outer radial shield, the present analysis will also apply to a boronated extended inner radial shield.

The purpose of using B_4C with a minimum stainless steel cover on the inside surface of the radial shield is to reduce the flux of neutrons backscattered into the plenum from the shield. If effective, the neutron flux will be significantly reduced at the top of the core catcher and at the PCRV liner above the wraparound shield. As a preliminary step, two one-dimensional slab models were calculated with the 1DFX code (Ref. 8-5), and the angular fluxes were printed. One model was calculated with a blanket spectrum impinging on a three-layered slab consisting of 12.7 mm of stainless steel, 150.0 mm of $B_4C + C$, and 150.0 mm of stainless steel. In the second model, graphite was used in place of the $B_4C + C$. The calculations with the $B_4C + C$ showed a very significant decrease in the backscattered neutron flux in the lower energy groups.

In the next step, a two-dimensional (RZ) model of the lower plenum was prepared, as shown in Fig. 8-5 (not to scale). This model was coded into the GA version of the DOT 3.5 code for calculations with $B_4C + C$

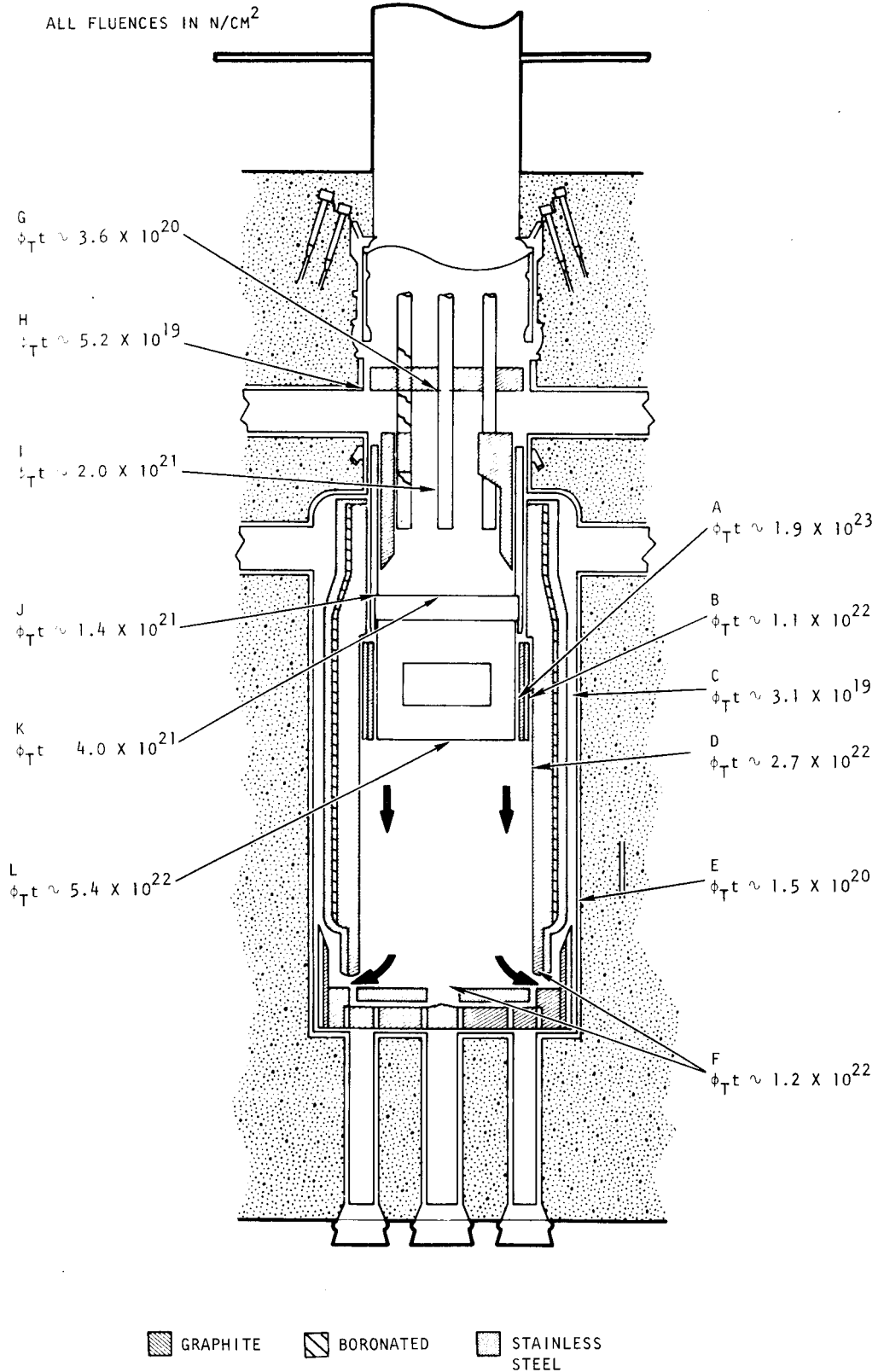


Fig. 8-4. Total neutron fluences at various points in the reference design, showing streaming paths in the lower plenum

Fig. 8-5. Model of lower plenum for streaming calculations

and graphite near the innermost radius. The changes in the neutron flux in the helium region will be of interest for ascertaining the effect of the B_4C on the streaming problem.

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- 8-3. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period August 1, 1977 Through October 31, 1977," DOE Report GA-A14613, General Atomic Company, November 1977.
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9. SYSTEMS ENGINEERING (189a No. 00585)

9.1. SYSTEMS DESIGN

During this quarter, preliminary overall plant performance at 25% load was established for a plant with electric-motor-driven circulators (Table 9-1). The preliminary Resource Evaluation and Control System (RECS) schedule input for this task was completed.

9.2. SYSTEMS INTEGRATION

The draft of the Baseline Data Book underwent general review, and comments are being incorporated.

TABLE 9-1
300-MW(e) GCFR DEMONSTRATION PLANT 25% POWER OPERATING CONDITIONS
WITH ELECTRIC-MOTOR-DRIVEN CIRCULATORS

	<u>25% Electrical Power</u>
Plant	
Electric power [MW(e)]	75.0
Net cycle efficiency (%)	32.6
Core	
Thermal power [MW(t)]	225
Helium pressure drop [MPa (psi)]	0.03 (3.9)
Helium flow rate [kg/s (lb/s)]	183.1 (403.7)
Inlet temperature [°C (°F)]	274.4 (526)
Outlet temperature [°C (°F)]	511.1 (952)
Circulator (1 of 3)	
Motor power [MW(e) (hp)]	0.75 (1000)
Helium flow rate [kg/s (lb/s)]	61.0 (1346)
Helium inlet pressure [MPa (psia)]	8.59 (1246)
Helium pressure rise [MPa (psi)]	0.03 (3.9)
Helium inlet temperature [°C (°F)]	272.5 (523)
Steam generator (1 of 3)	
Heat duty [MW(t)]	75.8
Helium flow rate [kg/s (lb/s)]	61.0 (134.6)
Helium pressure drop [MPa (psi)]	0.003 (0.4)
Helium inlet temperature [°C (°F)]	511.1 (952)
Water/steam flow rate [kg/s (lb/s)]	27.5 (60.6)
Feedwater pressure [MPa (psia)]	17.4 (2522)
Feedwater temperature [°C (°F)]	143 (289)
Superheated steam pressure [MPa (psia)]	17.24 (2500)
Superheated steam temperature [°C (°F)]	510 (950)
Secondary	
Throttle steam flow rate [kg/s (lb/s)]	82.4 (182)
Throttle steam pressure [MPa (psia)]	16.55 (2400)
Throttle steam temperature [°C (°F)]	507.8 (946)
Exhaust moisture (% at expansion line end point)	9.3

10. COMPONENT DEVELOPMENT (189a No. 00586)

10.1. REACTOR VESSEL

The scope of this subtask is to ensure that the design of the PCRV and related components which contribute to the integrity of the pressure boundary is satisfactory and to test critical component configurations to make certain that they attain the design objectives. This subtask will demonstrate by analyses and tests that the PCRV and its penetrations and closures meet the design criteria, and it will also ensure that (1) the design of the thermal barrier satisfactorily protects the liner and PCRV from the effects of high temperatures, and (2) the flow restrictors for the large penetrations can be developed to limit the flow of helium from the primary coolant systems to acceptable levels in the event of structural failure of a penetration or closure component.

During the last quarter, PCRV layout drawings were generated for configurations E-2 (Figs. 10-1, 10-2) and F-1 (Figs. 10-3, 10-4). Configuration E-2 incorporated the vertical circulator in the bottom head, and configuration F-1 incorporated the radial flow helium circulator. During this quarter the RECS schedule was revised to include changes in reactor vessel activities. The current status of the PCRV configurations is summarized in Table 10-1, which shows the design objective of each configuration along with a description and the size of the major reactor components or equipment and the reasons for proceeding to the next configuration. To incorporate the latest component and equipment designs, a new "G" series of PCRV arrangements will be prepared; design layouts have already been started for PCRV arrangements (Fig. 10-5) which accommodate an up-flow reactor core.

The preliminary structural analysis of the 1974 reference design demonstration plant (Ref. 10-1) found that the stress levels were acceptable.

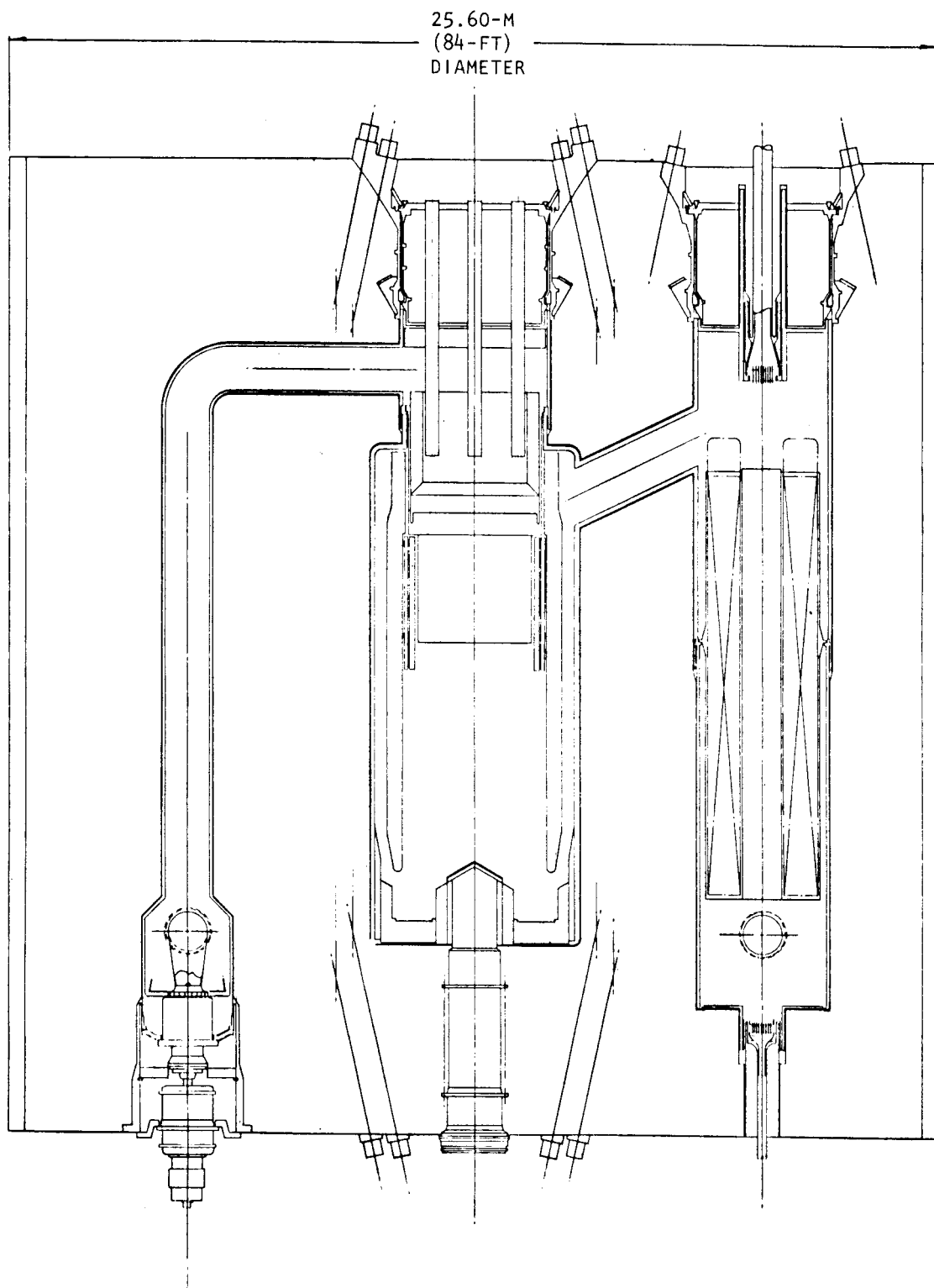


Fig. 10-1. Elevation view of PCRV with vertical circulator mounted in bottom head (E-2)

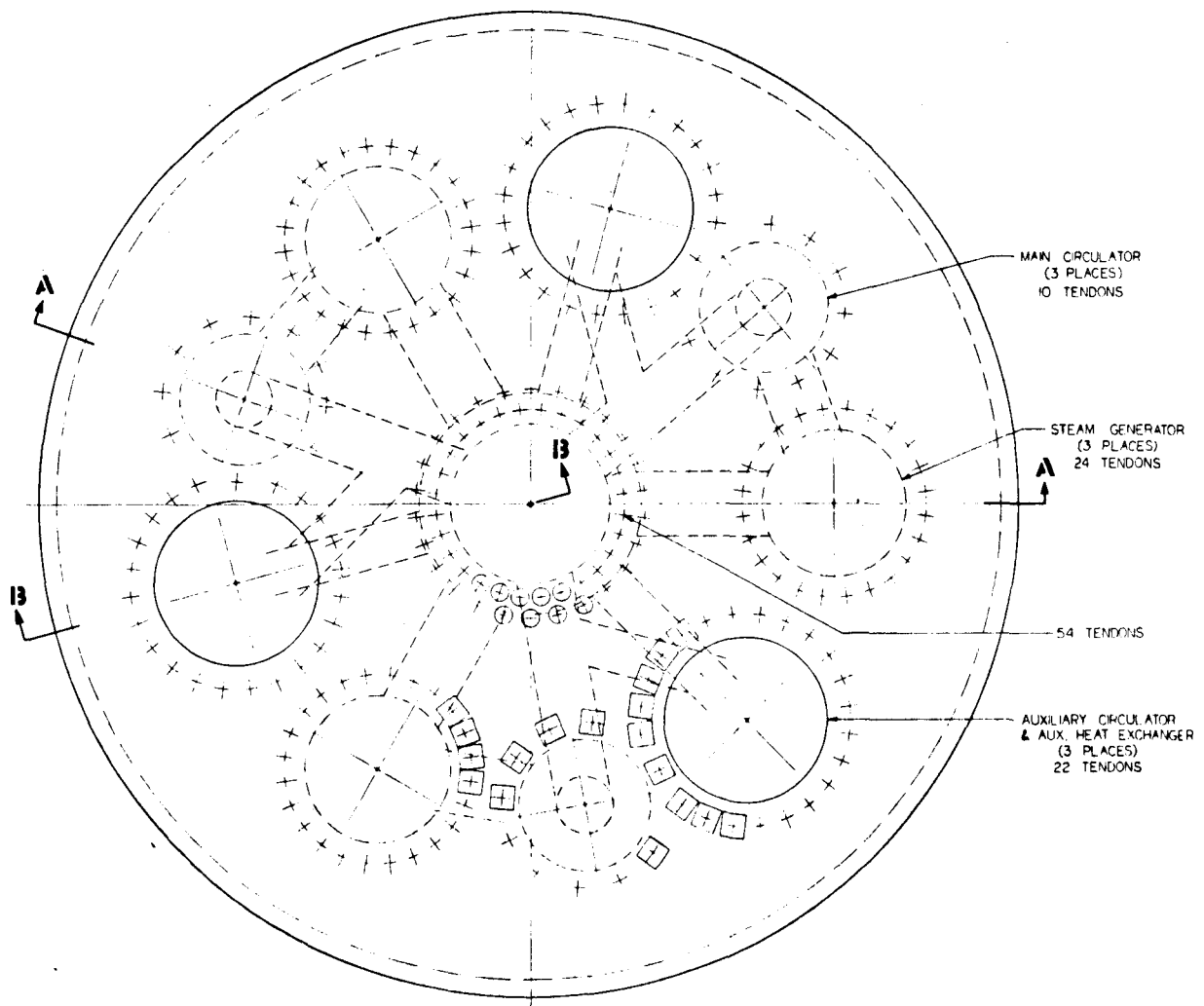


Fig. 10-2. Plan view of PCRV with vertical circulator mounted in bottom head (E-2)

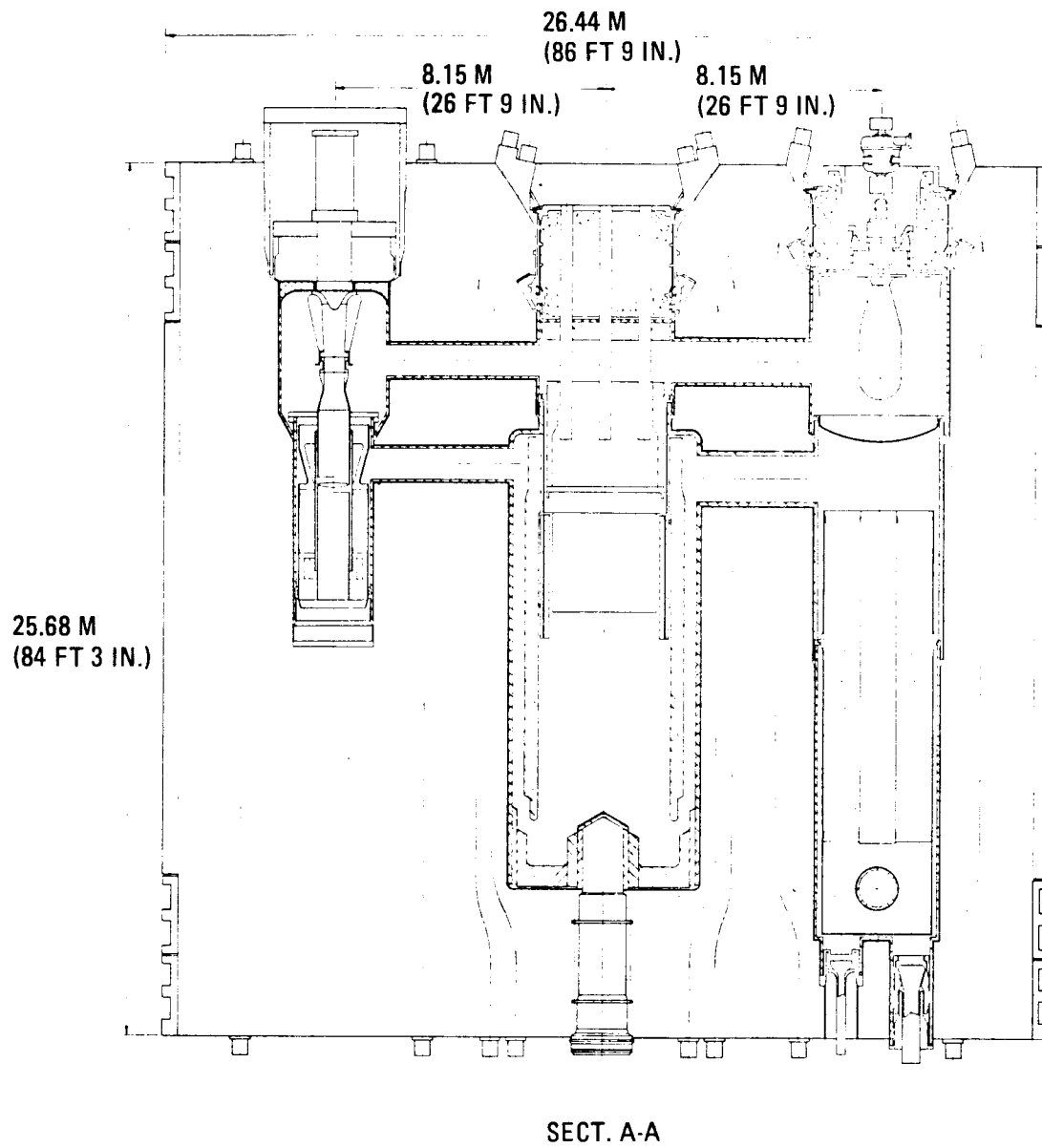


Fig. 10-3. Elevation view of PCRV with radial flow helium circulator in top head (F-1)

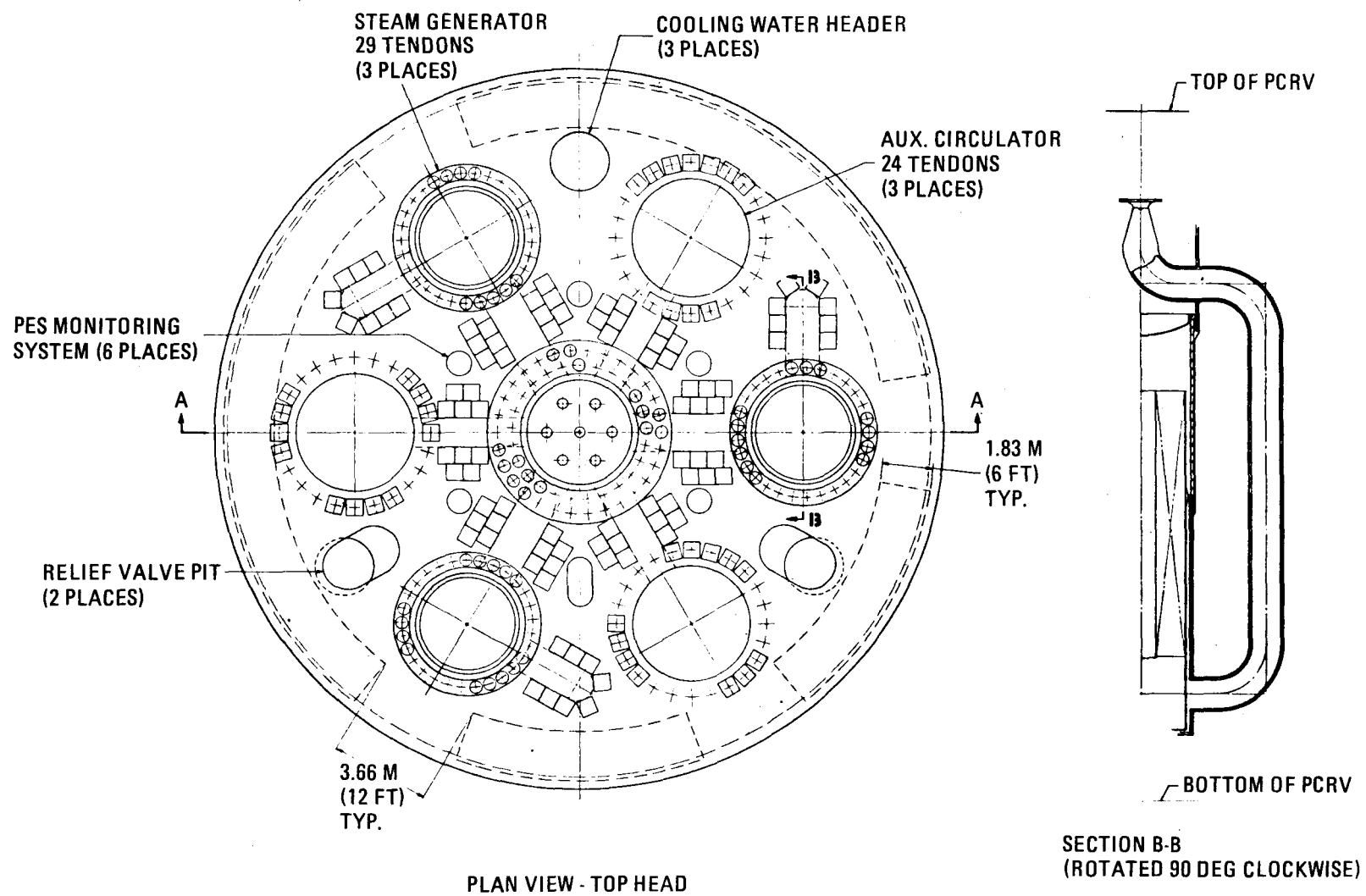


Fig. 10-4. Plan view of PCRV with radial flow circulator in top head (F-1)

TABLE 10-1
STATUS OF PCR V CONFIGURATIONS

Configuration	Objective	PCR V O.D. x Length x Cavity Diameter [mm (ft)]	Closure	Fuel Handling	Steam Generator	Core Assembly	Helium Circulator Type	Helium Circulator Drive	Auxiliary Circulator (Radial) [mm (ft)]	Core Catcher
C-2 BDB	Cost study of steam generator (resuperheater vs nonresuperheater)	25,603 x 25,375 x 5,740 (84 x 83.25 x 18.83)	Central with 27 control rod drives and 7 large penetrations	Through 3 bottom bottom penetrations	No resuperheater (center support); tube sheets in-service inspection tube sheets	Low ΔP (28)	Axial flow	Steam turbine	3,048 (10) diameter	None
E-1	Incorporation of core catcher and redesign of auxiliary circulator	26,441 x 26,594 x 5,740 (86.75 x 87.25 x 18.83)	Same as above	Through single penetration	Same as above	High ΔP (42) and corner assembly	Same as above	Same as above	4,572 (15) diameter	Yes
E-2	Separation of primary and secondary loops	25,603 x 26,670 x 5,740 (84 x 87.50 x 18.83)	Same as above	Same as above	Straight-through	Same as above	Radial; bottom-mounted	Electric	4,572 (15) diameter and in-service inspection	Yes
F-1	Incorporation of preferred radial flow helium circulator	26,441 x 25,679 x 5,740 (86.75 x 84.25 x 18.83)	Same as above	Same as above	No resuperheater (center support)	Same as above	Radial	Steam	Current design	Yes
G	Development of new PCR V configuration to contain latest component designs	To be determined; new upper plenum design	Central + 610 mm (2 ft); 19 control rod drives	To be determined	New design	New design	Redesigned radial	Electric	Bayonet	Yes

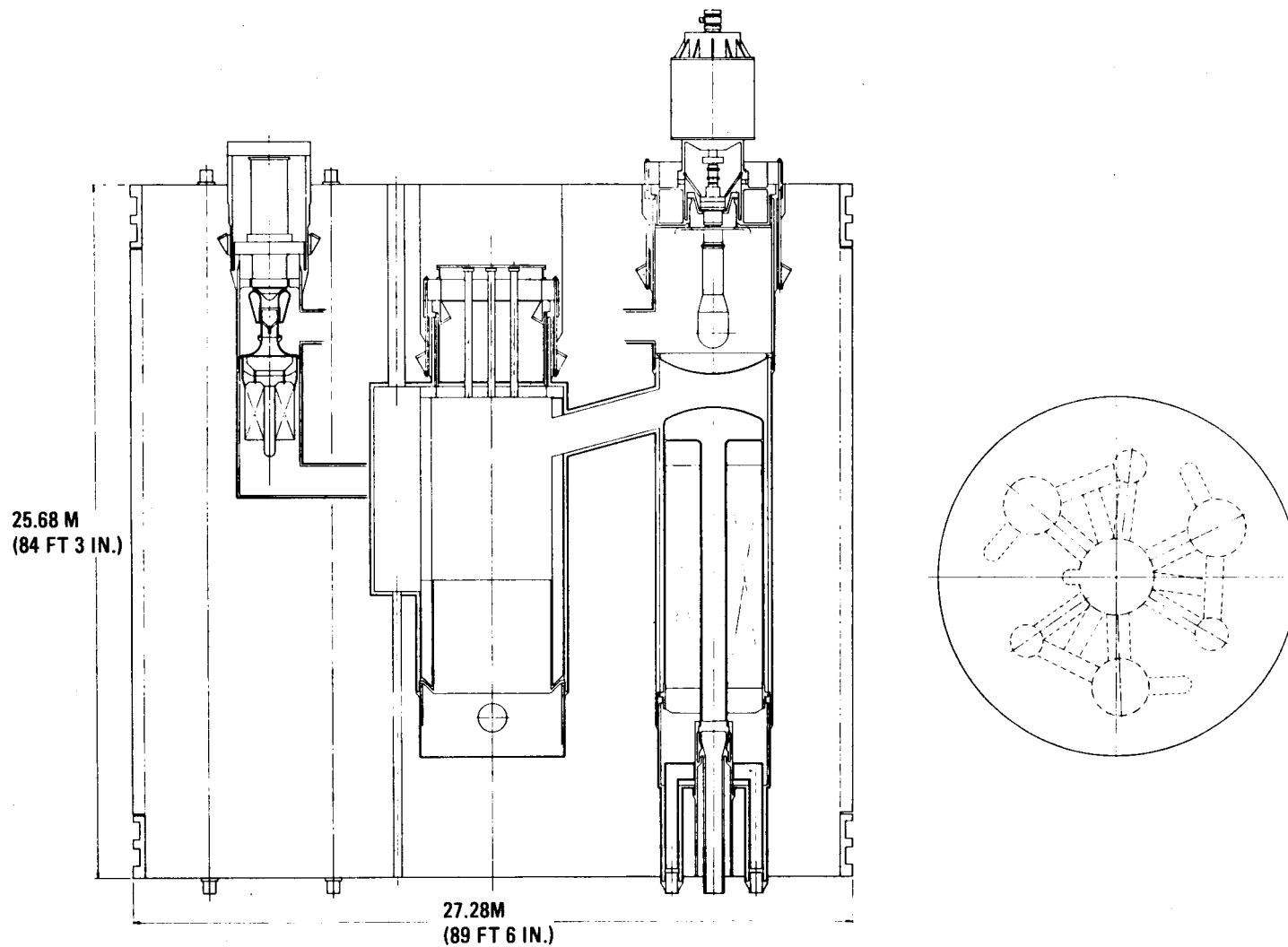


Fig. 10-5. PCRV configuration for up-flow core

However, the stress levels in the concrete ligaments between the steam generator and the core cavities indicate that the PCRV configuration can be improved by relocating the steam generator cavity farther from the vertical centerline. Increasing the inner ligament would also improve stress conditions at the top of the PCRV.

Conceptual design studies for a molten core retention and cooling system continued, with emphasis on core catcher concept envelope definition for two lower vessel configurations. A design layout was completed which defines the preliminary space envelope required for a high-temperature crucible type core catcher with a single refueling penetration. This configuration utilizes a buildup of ceramic materials to form an annular crucible that can contain a postulated 6.43 m^3 (227.1 ft^3) of molten oxide fuel at a depth of 0.415 m (16.2 in.) beneath a 3.0-m^3 (106-ft^3) pool of molten stainless steel.

Design studies were initiated to identify a viable concept for a multi-refueling penetration core catcher. Several design layouts have been completed, resulting in a crucible configuration consisting of a buildup of ceramic material with a 0.46-m (18-in.) thick mass of depleted uranium on top. A large portion of the magnesia has been replaced with depleted uranium which will melt during the initial stages of a postulated core meltdown and work its way down through gaps in ceramic materials, preventing lower-density oxide fuel from reaching the vessel liner. A preliminary space envelope drawing was completed, resulting in a configuration that will contain a 0.432-m (17.0-in.) deep layer of molten fuel beneath a 0.198-m (7.8-in.) deep layer of stainless steel.

The lower main cross duct (core to steam generator) will be studied with regard to the replaceable thermal barrier. This duct is 1.62 m (64 in.) in diameter (liner surface) and 3.21 m (126.5 in.) in length and may present some problems in terms of replaceability. Comparative designs of this duct were developed based on two preferred replaceable thermal barrier concepts concluded by previous HTGR studies to be the most promising (Ref. 10-2). One of these concepts consists of insulation modules compressed

by small square cover plates with only central attachments to the vessel liner. The alternate concept is based on larger rectangular cover plates which employ edge retainers in addition to the central fixture.

An overall review of major GCFR closures and the applicability of the ORNL closure test program to the present closure design was performed. It was found that the closure configurations are likely to change because of (1) changes to interfacing components, (2) the excessive strength shown in ORNL tests to date, and (3) the apparent inadequacy of the toggle hold-down system to withstand the maximum cavity pressure (MCP) with only 50% of the tendons available (an ASME Code required faulted condition). Recommendations were made on revision of the ORNL closure test program and pursuance of new closure and alternate hold-down system designs which are compatible with interfacing equipment and provide adequate but not excessive strength.

The present ORNL closure test program consists of 1/15- and 1/4-scale confirmatory tests of the steam generator cavity and core cavity upper closure designs. The smaller-scale tests are for demonstrating ultimate load capacity, and the larger tests are for demonstrating structural performance at loads up to 2 MCP. The completed 1/15-scale test of the steam generator closure showed no failure at a load of 7 MCP. A separate 1/15-scale model, identical to the first but with half the depth, failed in shear at a load of 9.75 MCP. Prior experimental data indicate that there is a need for directly applicable quantitative bases for the design of the GCFR closures. This need for more applicable experimental data together with the likely revision of interfacing components does not allow a firm design to be developed for confirmatory testing within a reasonable time span. Therefore, it was recommended that the 1/15-scale confirmatory tests be replaced by development tests of a comparable scale. Parameters recommended for study include closure depth/diameter ratio, number and size of control rod penetrations in core cavity closure models, and central penetration diameter/closure diameter ratio in steam generator cavity closure models. Pressure is to be applied to the external sidewalls and within control rod penetrations to reflect the significant difference found in

previous end slab tests. The need for confirmatory tests will be evaluated at a later time, but if required, may be made at a smaller scale than the 1/4-scale originally contemplated.

The most recent design of the steam generator closure provides a large central penetration to accommodate the new radial-flow, electric-motor-driven main circulator. Because the remaining ligament is small, a concrete closure will not meet current design criteria. Therefore, for the present, a steel closure design based on earlier analyses performed on similar configurations for use on the HTGR has been adopted. The final choice of either a steel or a concrete closure will depend in part upon the results of the ORNL test program.

A number of conceptual configurations for the core cavity closure were designed and analyzed to determine the proper interface of the cavity with the control rod and core arrangements. It was established that a minimum ligament of 127 mm (5 in.) allows for an adequate cooling tube arrangement, reasonable fabrication procedures, and acceptable concrete placement techniques and meets all allowable stress limits. A design was developed with thirty-seven 356-mm (14-in.) diameter penetrations at a 483-mm (19-in.) pitch. The depth of the closure was set at 3.66 m (12 ft) with the expectation that this may be reduced when the ORNL development tests are completed.

An initial assessment of the reference steam generator cavity and core cavity closure hold-down system was completed. In this system, developed in Sweden, multiple toggle bars transfer the pressure loads from the closure to a large upper ring forging, which in turn is directly held down by the PCRV linear prestressing tendons. Multiple rings are used within the closures as shear anchors, and a bayonet lock is provided as a limit stop in the postulated event of a failure within one of the primary hold-down components preventing that component from functioning. A flow restrictor is also necessary under this postulated condition. Preliminary analysis has shown that the reference configuration is not capable of withstanding MCP with only 50% of the tendons. This failure load combination is required

by the ASME Code, Section III, Division 2. Although it is possible to strengthen the system, the required size of the upper ring forging exceeds current manufacturing capabilities. Since the toggle bar system is costly and difficult to fabricate and align and leads to large strain concentrations at the junction of the liner and ring forging, it is recommended that alternate hold-down systems be investigated. The bayonet lock requires a precision alignment between the closure, cavity liner, and core assemblies which will be very difficult to attain. It also requires complex forgings in the closure and cavity liners and a flow restrictor which, based on prior HTGR experience, may be difficult to design. Therefore, it is also recommended that alternatives to the bayonet lock (along the lines of the most recent HTGR designs) be investigated. The initial assessment of these closures and hold-down systems was completed (Ref. 10-3).

The auxiliary cooling loop cavity increased in diameter owing to the use of a radial flow type circulator. Although the diameter may change further as a result of optimization, it is likely that a single plate will not be adequate for use as the near-zero leakage flow restrictor and limit stop. Therefore, preliminary studies on the use of an ellipsoidal dome for this flow restrictor were initiated.

10.2. CONTROL AND LOCKING MECHANISMS

During this quarter a new reference design control rod pattern was adopted. The new arrangement reduces the number of control rods from 27 to 19 and locates them in the centers of regions consisting of seven core assemblies. The primary reason for this change was to provide the core geometry necessary to accommodate the new core locking and reactivity control mechanisms concept. Basically, the core locking concept operates on the principle of self-energization of clamping forces to the grid plate produced by core assembly weight and flow pressure drop. The clamping mechanism is an integral part of each core assembly. Engagement and release of the clamping action is performed with the core assembly in its unloaded state, such as when it is supported during a refueling operation.

The engagement and release operation for the seven core assemblies of each region is accomplished by mechanisms within the centrally located control rod drive assembly. The control rod drive assembly provides thermocouple pathways to each of the core assemblies in a region. In the blanket zone of the core, 18 additional lock actuation mechanism assemblies are distributed in the same pattern as the control rod drive installations. These mechanisms also provide thermocouple pathways to the blanket assemblies.

The original reference design had a core locking concept based on individual support mechanisms for each core assembly extending up and through the PCRV top head closure. The core assemblies were held to the grid plate against the separating forces from weight and pressure drop by preloaded tension members. With the current core configuration, 217 top head penetrations would be required for that core locking concept; the new concept requires 37 penetrations. Continuing design development studies on the new core locking concept do not indicate any basic interface difficulties with the PCRV and internals. The major interface impact is with the core assembly design. This interface area will be examined.

10.3. FUEL HANDLING DEVELOPMENT

The preliminary RECS input on schedule, manpower, and budget was completed for the fuel storage and shipping equipment. A preliminary status report for the conceptual design of a single refueling machine which utilizes a single-bottom PCRV refueling penetration was prepared to document the status of feasibility studies (Ref. 10-4). It appears that a single machine can be used for refueling and fuel transfers. However, an additional penetration is required for emergency viewing and servicing. Additional studies and coordination are required to make final recommendations to support this scheme.

10.4. CORE SUPPORT STRUCTURE

The purpose of this subtask is to assure the availability of the structural analysis methods and 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.

Work accomplished during last quarter included the final phase of the preliminary seismic analysis of the core support structure, including the effects of the core assemblies, and the preliminary thermal stress analysis of the grid plate (Refs. 10-5 through 10-7). During this quarter the feasibility of a core support structure for an up-flow configuration was studied.

10.4.1. GCFR Core Support Structure Up-Flow Configuration Study

The salient features of the up-flow configuration compared with the reference design are as follows:

1. Core constraints have been provided in the form of a core barrel and core formers.
2. The grid plate consists of two parts. The thicker upper plate serves as the primary load-carrying member, and the thinner bottom plate serves as the core assembly mounting device. There are cylindrical shells around each core assembly, spanning the upper and lower plates. The shells serve as part of the structure as well as the housing of the pressure balance tubing. The thickness of the grid plate for the down-flow core is 609.6 mm (24 in.). Because there are core restraints in the proposed up-flow configuration, the reactivity change of the core is not directly related to the deflection of the grid plate, and therefore the grid plate can be designed to be thinner. At present, it is proposed that the upper grid plate be approximately 304.8 mm (12 in.) thick and the bottom plate about 152.4 mm (6 in.) thick. The effective thickness of the bottom plate is about 50.8 mm (2 in.) owing to the presence of a large number of holes.

3. The upper plate is supported by a conical shell structure from the PCRV liner. This conical shell contains a series of small holes through which bypass helium flows and cools the shielding and core barrel.

10.4.2. Thermal Stress in the Grid Plate Due to Reactor Trip

A simplified thermal and stress analysis was performed for the grid plate for the transient conditions following reactor trip. The results of the analysis show that the stress caused by a reactor trip with an electrically driven circulator is acceptable. The fatigue damage to the grid plate by 300 such events over the life of the plant is small and well below the acceptable limit. The same condition (reactor trip) was also investigated for the case of a steam-driven circulator. It was found that the transient is much less severe than that with an electric circulator, and the resulting stresses in the grid plate are very low.

10.5. REACTOR SHIELDING ASSEMBLIES

The purpose of this task is to design and develop analytical methods and experimental programs to evaluate the reference design of the reactor shields. This evaluation is expected to cover nonuniform temperature distribution, material behavior, seismic effects, hydrodynamic tests, and structural analysis. The design study also includes alternate shield configurations so that a satisfactory and optimized shield design can be developed.

During the previous quarter, the recommended design criterion for the residual total elongation (RTE) of the stainless steel components of the inner radial shield (IRS) was reduced to 1% based upon examination of the requirements for replaceable components. In addition, a concept was developed for making the IRS supports replaceable at refueling intervals, since the assessment of radiation damage potential indicated that the radiation levels in the area of the supports would exceed allowable levels for a 30-yr life.

During this quarter, preliminary layouts were developed of a method of supporting the outer radial shield (ORS) from the liner. The layouts also included an axially segmented IRS which extended to the lower extremity of the ORS and was supported from the ORS by means of replaceable supports. Preliminary load and stress analysis was begun. The purposes of these layouts are as follows:

1. To assist in developing manufacturing methods for the steel-encased graphite components of the IRS and ORS.
2. To quantify the loads being applied to the PCRV.
3. To permit preliminary flow and thermal analyses to begin.
4. To permit preliminary stress analysis of the IRS, ORS, and supports.
5. To develop a method for axially segmenting the IRS to provide full-length protection for the ORS while permitting removal and replacement through the refueling penetration.

A layout was produced in support of the up-flow core feasibility study. In this layout, the ORS was placed near the thermal barrier and was cooled by allowing 2% of the (cool) inlet gas to bypass the core through orifices in the conical core support structure and to flow upward over both sides of the ORS. The replaceable shielding was put inside the core barrel and supported by the grid plate.

10.6. MAIN HELIUM CIRCULATOR, VALVE, AND SERVICE SYSTEM

The purpose of this subtask is to develop the helium circulator, its service system, and main loop isolation valve to demonstrate performance and reliability by testing under anticipated operating conditions. The overall objective for FY-78 is to continue first-of-a kind conceptual design

and performance analysis of the circulator reference design configuration (external steam drive), service system, loop isolation valve, and alternate electric drive. Preliminary layouts of the circulator components will be made and requirements established for bearings, shaft critical speeds, seal flow rates, helium buffer systems, drains, jet pumps, inlet, diffuser, compressor, shaft coupling, rotor dynamics, and aerodynamic performance. The work outlined will provide input to the circulator pretest analysis task.

10.6.1 Main Helium Circulator

A number of vendors were asked to bid on the preliminary design of a steam compressor for a test facility to test a circulator driven by a steam turbine. All vendors contacted declined to bid. Several vendors were asked to bid on the preliminary design of an electric motor and a variable-frequency power supply to be used in a test facility for a circulator with an electric motor drive; one vendor was chosen. Consequently, a decision was made to concentrate work efforts on the electrically driven radial flow circulator with no provision for overspeed during DBDA. The effect of this design on overall system performance and safety will be reviewed.

The RECS schedule and the development plan for the GCFR main circulator were updated to include alternate circulator conceptual design studies and development of an electric motor drive. Adjustments were made to the overall schedule to include the impact of switching to an external electric drive.

The performance of the conceptual centrifugal compressor for a 4500-rpm electric motor drive was calculated for the design point case and selected off-design points. The off-design points explore the range of operation to set the scope of a full operating map.

10.6.1.1. GCFR Main Helium Circulator Design Verification and Support. A specification has been prepared for tests of the GCFR main circulator (Ref. 10-8). This specification will serve as the basis for a conceptual test facility design to be carried out by an architect-engineer. A

preliminary plant layout and layout criteria were completed for the pressure vessel to be used in the GCFR test facility.

10.6.1.2. Computation Methods. A new computer program PREDM (Ref. 10-9), used to predict the design point and off-design performance of centrifugal compressors, was applied to analysis of conceptual designs of the GCFR main and auxiliary circulator compressors. The results of journal bearing analysis methods used by GA were compared with vendor calculations for the GCFR drive motor oil bearings. There was good agreement between the GA and vendor results.

10.6.1.3. Bearings and Seals. The compressor assembly (described in Ref. 10-10) will use water bearings because it is the preferred interface fluid with PCRV internals.

Several types of high-pressure rotating shaft seals have been investigated for the compressor assembly. These seals would utilize compressor bearing water as the sealing medium. A multiple mechanical face seal similar to the type used in LWRs is preferred because

1. Very low leakages [~ 4 liters/min (~ 1 gpm)] are possible.
2. There are operational and licensing precedents in LWR applications.
3. The seals are available in cartridge form and would require minimal development.

Since there is some rubbing in these seals, they must be periodically replaced. The average seal life in LWR applications is 2 yr. With the cartridge design, the seal can be changed in as little as 2 hr. The remainder of the water bearing arrangement is similar to that developed for the HTGR, and since there are no rubbing surfaces, it is designed to operate without maintenance for the life of the plant.

The rotor dynamics of the 300-MW(e) GCFR (demonstration plant) main helium circulator was analyzed. The rotor was modeled as being supported by two flexible bearings resting on two very stiff pedestals. Previous studies on the HTGR main circulator housing supports indicated that the natural frequencies of these pedestals were above 170 Hz and would therefore not significantly affect the critical speeds of the rotor bearing system. To attain a critical speed of 5400 rpm for the compressor shaft, it was necessary to use bearing stiffnesses of 1.751×10^9 N/m (10×10^6 lb/in.). The critical speed of the circulator assembly with the electric motor drive was also found to be 5400 rpm. Further work is being planned to optimize the shaft design.

10.6.1.4. Main Circulator Aerodynamic Design Studies. The PREDM program made it possible to perform more accurate predictions of the off-design performance of the compressors. Two conceptual designs produced during last quarter for drives at 4500 and 3600 rpm at the design point were analyzed using the code. The results of the design point analyses are summarized in Table 10-2, which lists the overall geometry of the two designs, the critical working fluid parameters at various points in the compressors, the efficiency of the compressors, and the efficiency losses within the compressor. There is very little difference between the basic efficiencies of the compressors, i.e., the efficiencies calculated through the vaned diffuser exit. However, in both cases, the loss at the exit from the vaned diffuser is considerable, i.e., about eight percentage points. This is because all the energy in the fluid at the vaned diffuser exit is lost as the helium is dumped into the plenum around the circulator. This is a conservative assumption in that some of the energy may actually be recovered. Means of increasing the efficiency are to increase the outer diameter of the vaned diffuser, lowering the exit velocity, or to add an annular diffuser following the turn from the vaned diffuser exit. It should be pointed out that these are first conceptual designs; refinements will be investigated as the design is developed. The off-design performances of the conceptual compressors are shown in Figs. 10-6 and 10-7, which are performance maps calculated for the design point inlet temperature and

TABLE 10-2
GCFR MAIN HELIUM CIRCULATOR DESIGN STUDY
OF CENTRIFUGAL COMPRESSORS

	Design Point Rotational Speed	
	3600 rpm	4500 rpm
Dimensions		
Impeller tip diameter [mm (in.)]	1549.4 (61.00)	1210 (47.64)
Impeller inlet shroud diameter [mm (in.)]	736.6 (29.00)	660 (26.00)
Impeller inlet hub diameter [mm (in.)]	368.3 (14.50)	220 (8.667)
Impeller exit width [mm (in.)]	65.3 (2.57)	79.4 (3.125)
Impeller exit angle (deg from radial)	0	0
Impeller inlet shroud angle (deg from axial)	35	35.77
Impeller inlet hub angle (deg from axial)	37	29.83
Diffuser vane inlet radius [mm (in.)]	885.4 (34.85)	691.6 (27.23)
Diffuser vane exit radius [mm (in.)]	1346.2 (54.00)	1140 (44.91)
Diffuser vane inlet angle (deg from radial)	67.15	65.6
Diffuser vane turning angle (deg)	24.6	21.5
Diffuser mean channel length [mm (in.)]	624.8 (24.60)	533.4 (21.0)
Number of impeller vanes	29	28
Number of diffuser vanes	33	23
Performance		
Inlet shroud relative velocity [m/s (ft/s)]	240.5 (789)	226 (873)
Inlet hub relative velocity [m/s (ft/s)]	114.6 (376)	104.2 (342)
Exit absolute velocity [m/s (ft/s)]	289.8 (950.7)	285 (934.9)
Losses (%)		
Impeller skin friction	2.22	2.04
Impeller blade loading	2.61	3.17
Impeller blade wake mixing	0.19	0.62
Vaneless diffuser	2.37	1.54
Vaned diffuser	6.71	6.91
Exit velocity dump	7.91	8.31
Disk friction	0.37	0.24
Leakage	1.55	1.54
Efficiency (%)		
To vaned diffuser exit	83.97	83.93
After diffuser exit	76.06	75.62

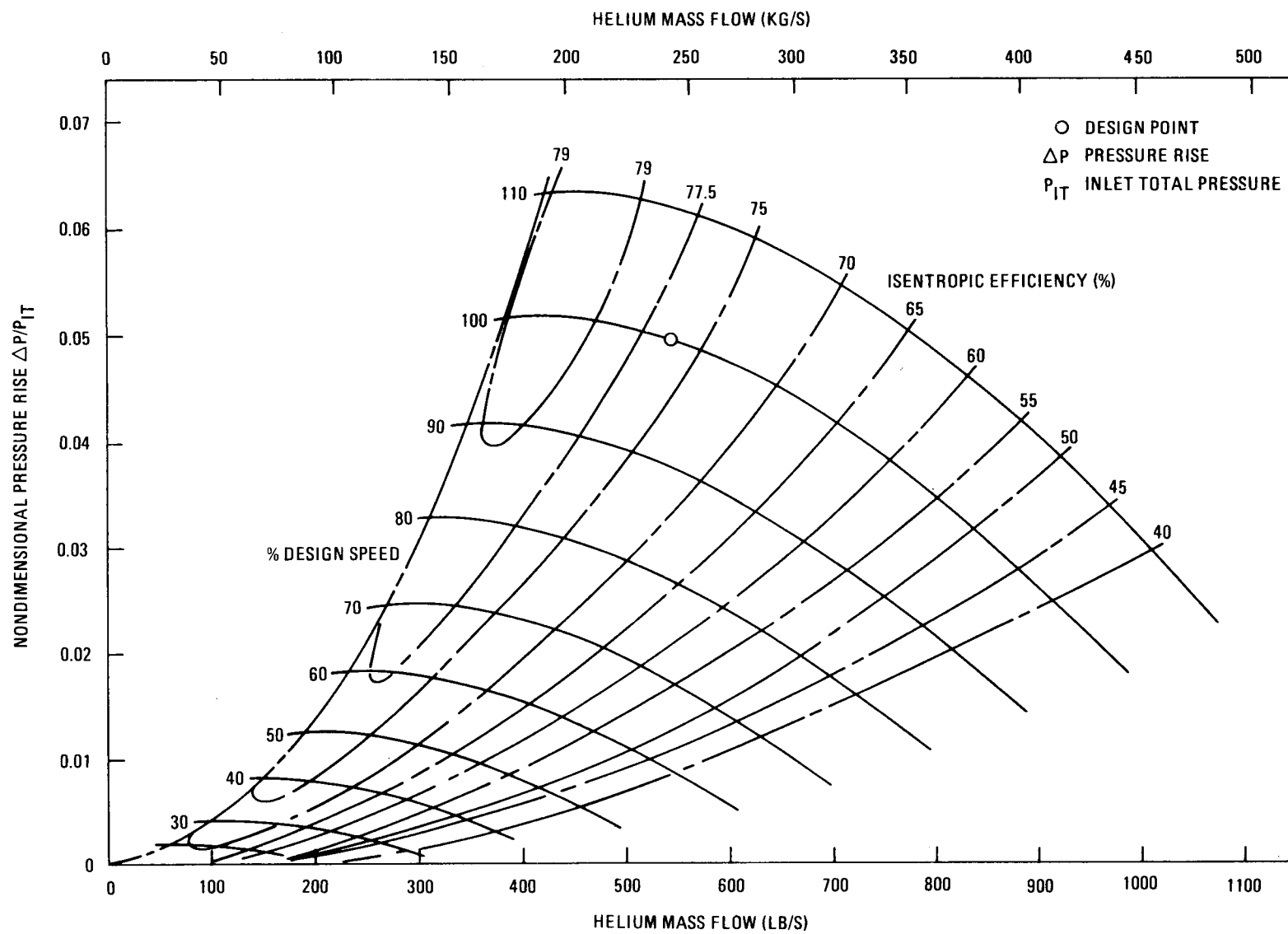


Fig. 10-6. Off-design performance of 3600-rpm circulator

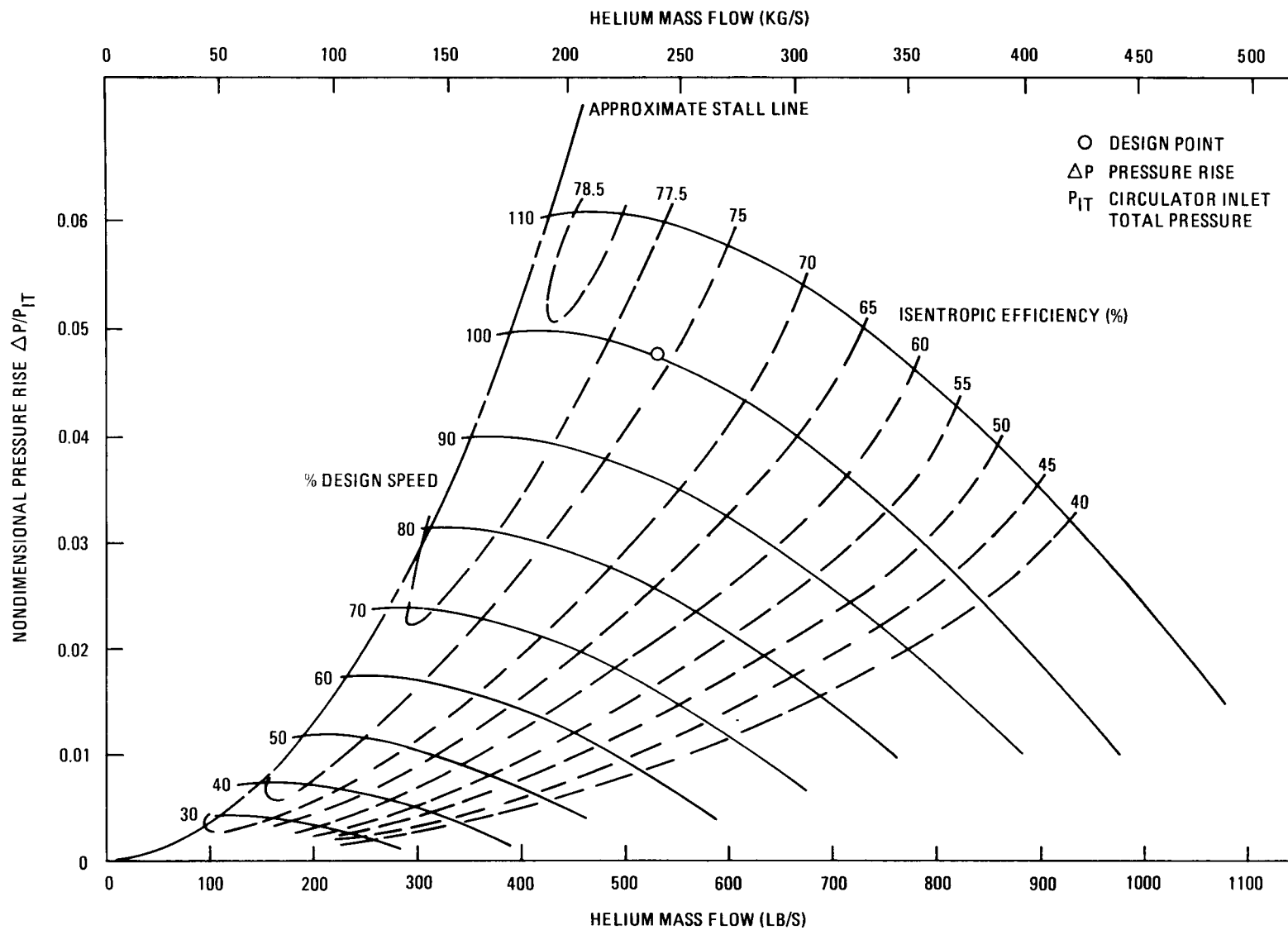


Fig. 10-7. Off-design performance of 4500-rpm circulator

pressure. They therefore represent the performance of the circulators under pressurized reactor conditions.

10.6.2. Electric Motor Drives

A preliminary design of an external electric drive main circulator is shown in Fig. 10-8. A study of the main 18-MW drive motor is in progress to provide more details on the synchronous motor which has a two-pole solid rotor and a full-power operating speed of 4500 rpm. This high operating speed is desirable for compressor matching and small PCRV penetrations. Because the circulator must operate at all speeds, the rotor assembly first critical speed will be at least 15% above the maximum speed. A lower-speed 100-KW pony motor is mounted on top of the main motor for use during certain residual heat removal conditions. The pony motor drives the circulator through a mechanical clutch directly connected to the main motor shaft which in turn drives the compressor through a rigid coupling. The coupling has a removable section so that the compressor shaft seal can be replaced without removing either the motor or the compressor. A single thrust bearing will be located in either the motor or the compressor assembly.

10.6.3. Main Loop Isolation Valve

Figure 10-9 shows a feasible main loop isolation valve concept for the reference design circulator. It consists of a flow-actuated butterfly valve located upstream of the main circulator inlet with an electromechanical override. The valve blade is supported by roller bearings mounted in the valve support structure. The valve override mechanism acts on the valve blade through a side penetration and is mounted on the PCRV periphery on top of a land area in between the prestressing grooves. The mechanical valve is actuated by an electrical motor operating a gear reducer, the overriding mechanism, and an actuation rod. The limit switches located inside the actuator housing are operated by the overriding mechanism and are used for valve position indication. The valve assembly can be remotely removed through the circulator penetration by the circulator handling equipment.

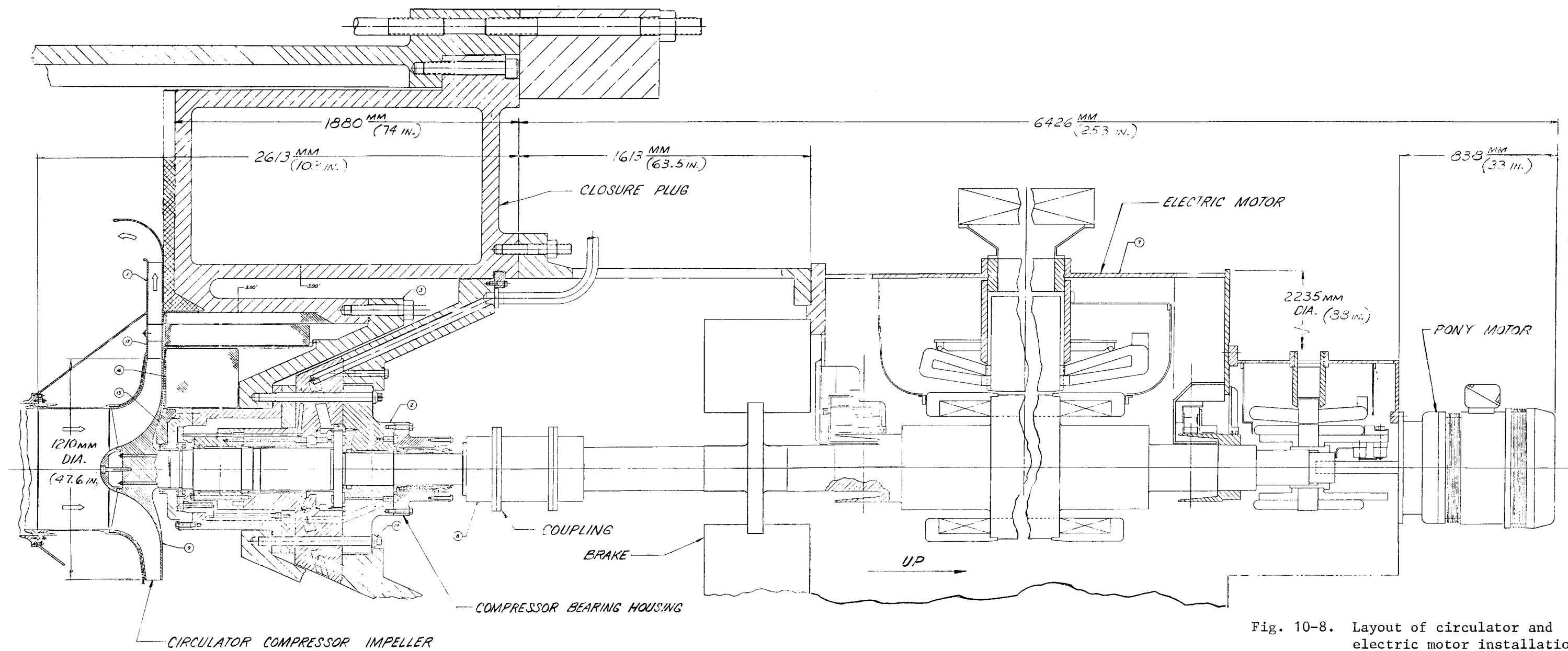


Fig. 10-8. Layout of circulator and electric motor installation

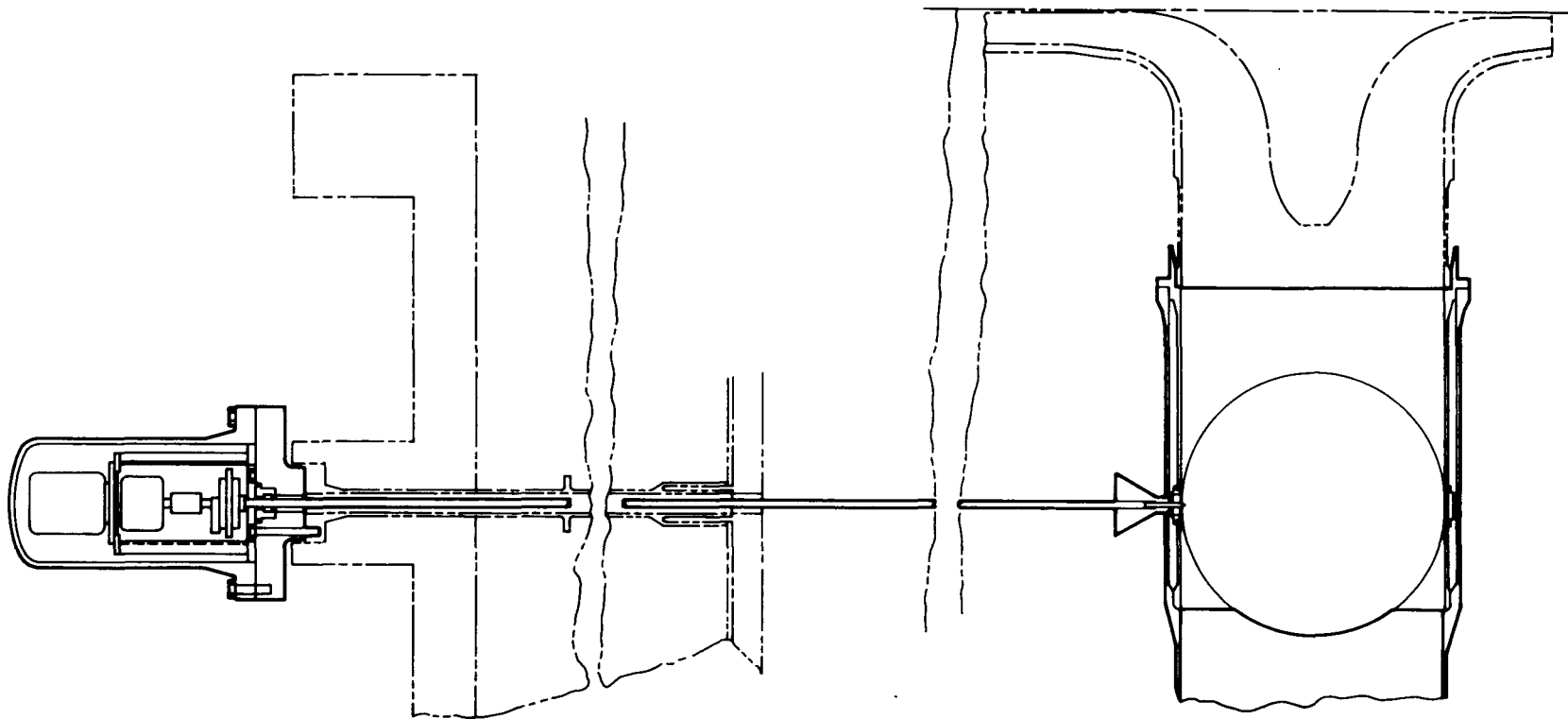


Fig. 10-9. Layout of updated main loop isolation valve

10.7. STEAM GENERATOR

The objective of this task is to design and develop a steam generator which meets the operational, performance, and safety requirements of the GCFR. A once-through steam generator was selected as the concept best suited to meet the design requirements. The current design utilizes a helically coiled main steam bundle with straight superheater lead-out tubes. The steam generator consists of an economizer, an evaporator, and a superheater section; there is no resuperheater. The feedwater enters from the bottom of the steam generator. The helium flow path is arranged to form a countercurrent pattern with respect to water flow direction. Accordingly, helium flows downward from the top of the steam generator, out from the bottom, and then up through a separate ducting to the main circulator.

The 100% load conditions for the steam generator and the circulator drive system are identical. Since the design effort concentrates on optimizing the conceptual arrangement of the steam generator to minimize uncertainty and design difficulty, two major changes which offer considerable advantages were proposed. The first change involves lowering the main support flange to the bottom of the bundle. This change in main support flange position has no effect on providing adequate seismic support, but it does maintain the bundle support structure in a complete compression load path, which definitely eases the in-service inspection requirements and enhances confidence in the support structure design. The second change involves placing the superheater tube sheet in the center of the bottom penetration, eliminating the lower expansion loops extending from the end of the straight lead-out tubes to the top of the tube sheet. The expansion loops must be designed to provide freedom for thermal expansion on the one hand and sufficient support for seismic load on the other. To satisfy these two opposing requirements, the design must go through numerous iterations. Thus, eliminating the lower expansion loops allows the design effort to be concentrated on only the top expansion loops. The centered tube sheet arrangement is made possible by splitting the feedwater

tube sheet into two smaller ones to maintain minimum PCRV ligament requirements. A detailed study of the PCRV is being carried out to confirm the feasibility of the twin feedwater penetration arrangement. Figure 10-10 shows the schematic arrangement of the steam generator with the twin feedwater tube sheets on two sides of the superheater tube sheet; it also shows the main support flange located near the bottom of the bundle.

The steam generator component reference materials were identified. Changes and revisions are expected as more analyses are performed. The reference materials for major components are listed in Table 10-3. The most significant development is the requirement for alloy 800H for superheater tube sheets. Since the tubing material is presently 2-1/4 Cr - 1 Mo, the alloy 800H tube sheet introduces the requirement of a bimetallic weld. The current junction of the two metals is placed on the straight tube section inside the tube sheet. Further study is required to confirm the feasibility of this arrangement.

A preliminary sizing analysis based on expected conditions was completed (Table 10-4). The analysis was based on a given envelope and support plate ligament efficiency. The bundle size includes average fouling, 1% tube plugging, and support plate coverage. The tube length was maintained at a reasonable value of approximately 126 m (412 ft), although this was not considered to be as a constraint. The tube o.d. was kept uniform, which is a preferred requirement for tube coiling. A special feature of the bundle dimension is its large outer to inner coil diameter ratio, which is approximately equal to 3.7. Since a large ratio results in large bear-hug stress on the tube bundle, a CSTRES analysis (Ref. 10-11) must be performed to confirm that the heat exchanger dimensions are correct. The low helium to tube wall temperature difference, however, considerably reduces the probability of the bear-hug stress.

The significance of an electric-motor-driven circulator is that it eliminates the 2% flow requirement for a reactor shutdown transient. Therefore, a low-flow boiling stability test is not presently scheduled.

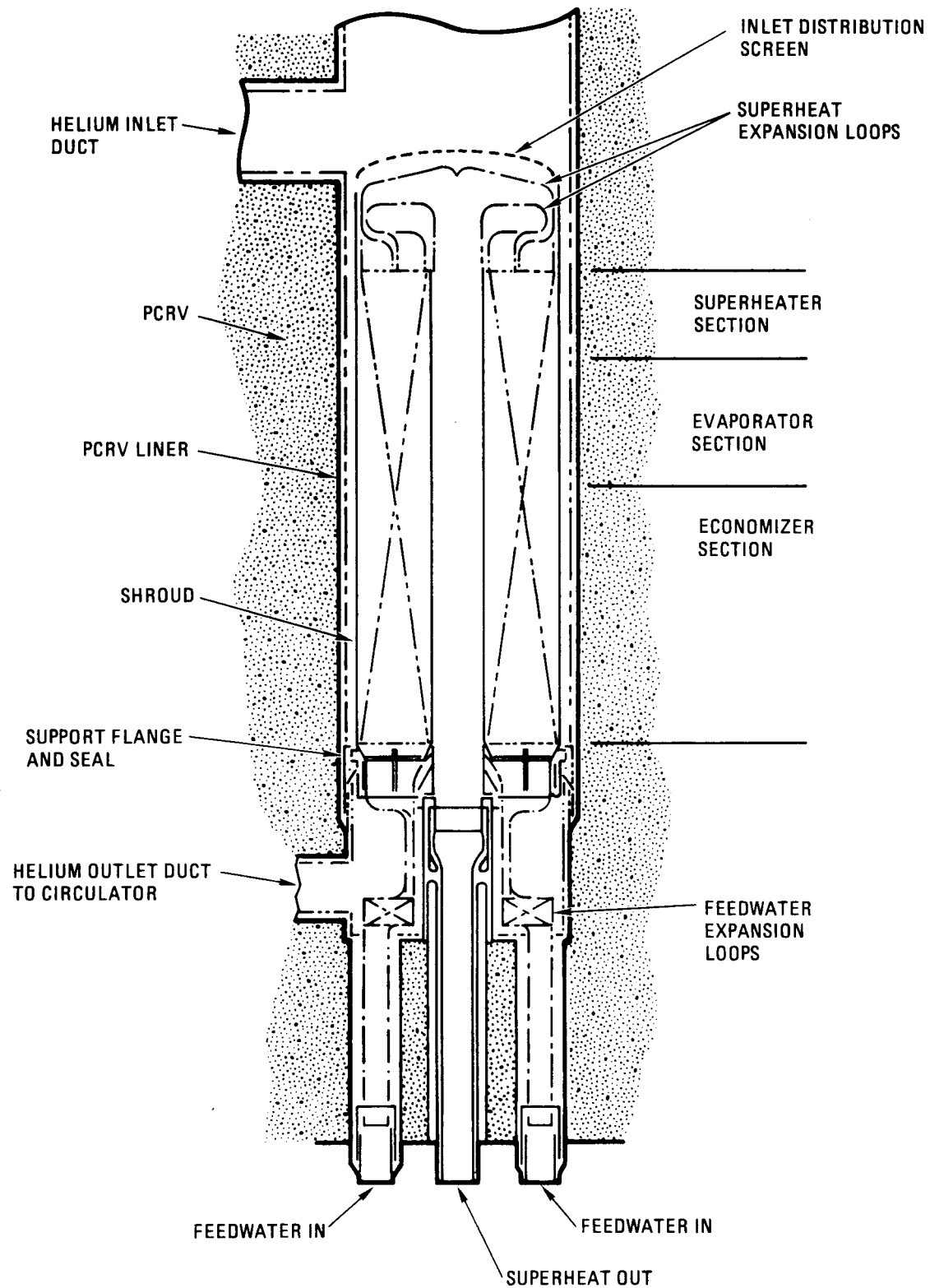


Fig. 10-10. Steam generator

TABLE 10-3
STEAM GENERATOR REFERENCE MATERIALS FOR MAJOR COMPONENTS

Component	Material	ASME Material Specification
Main support flange	2-1/4 Cr - 1 Mo	SA-336 CL F22a
Outer shroud	2-1/4 Cr - 1 Mo	SA-387 Gr22 CL1
Inner shroud	2-1/4 Cr - 1 Mo	SA-387 Gr22 CL1
Bundle support plate	2-1/4 Cr - 1 Mo	SA-387 Gr22 CL1
Tube support grids, gussets	2-1/4 Cr - 1 Mo	SA-387 Gr22 CL1
Evaporator-economizer-superheater bundle and superheater-downcomers	2-1/4 Cr - 1 Mo	SA-213 Gr T22
Feedwater lead-in support	2-1/4 Cr - 1 Mo	SA-387 Gr22 CL2
Superheater tube sheet	Alloy 800H	SB-564 solution-treated Ni-Fe-Cr alloy
Superheater tube sheet reducer	Alloy 800H	SB-564 solution-treated Ni-Fe-Cr alloy
Superheater tube sheet support	Alloy 800H	SB-564 solution-treated Ni-Fe-Cr alloy
Feedwater tube sheet	Carbon steel	SA-508 CL2

TABLE 10-4
SIZING INFORMATION

100% load conditions	
Helium (each loop)	
Inlet helium flow [kg/s (lb/hr)]	236.5 (1.877 x 10 ⁶)
Inlet helium temperature [°C (°F)]	551.7 (1,025.0)
Inlet helium pressure [MPa (psia)]	8.63 (1,252.0)
Helium pressure drop [MPa (psi)]	0.04 (6.0)
Feedwater (each loop)	
Inlet feedwater flow [kg/s (lb/hr)]	126.6 (998,280.0)
Inlet feedwater temperature [°C (°F)]	210.0 (410.0)
Inlet feedwater pressure [MPa (psia)]	19.65 (2,850.0)
Main steam (each loop)	
Outlet main steam temperature [°C (°F)]	510.0 (950.0)
Outlet main steam pressure [MPa (psia)]	17.24 (2,500.0)
Preliminary bundle sizing data	
Active surface area [m ² (ft ²)]	3,660 (39,399)
Installed surface area [m ² (ft ²)]	3,855.4 (41,499)
Number of tubes	385
Number of columns	34
Bundle o.d. [mm (in.)]	356 (140)
Bundle i.d. [mm (in.)]	96 (38)
Bundle height [mm (ft)]	8,230 (27)
Transverse tube pitch [mm (in.)]	381 (1.5)
Longitudinal tube pitch [mm (in.)]	381 (1.5)
Superheater tube diameter [mm (in.)]	254 (1.0)
Evaporator tube diameter [mm (in.)]	254 (1.0)
Economizer tube diameter [mm (in.)]	254 (1.0)
Helium ΔP, bundle [MPa (psi)]	0.026 (3.7)
Helium ΔP, other [MPa (psi)]	0.012 (1.8)
Steam/water ΔP, bundle [MPa (psi)]	1.138 (165)
Steam/water ΔP, orificing/lead-outs [MPa (psi)]	~1.276 (~185)
Length of tubes [mm (ft)]	1.256 x 10 ⁵ (412)
Superheater tube wall thickness [mm (in.)]	4.67 (0.184)
Evaporator tube wall thickness [mm (in.)]	4.52 (0.178)
Economizer tube wall thickness [mm (in.)]	4.52 (0.178)
Superheater fouling conductance [W/m ² -°C (Btu/hr-ft ² -°F)]	45,426 (8,000)
Evaporator fouling conductance [W/m ² -°C (Btu/hr-ft ² -°F)]	28,391 (5,000)
Economizer fouling conductance [W/m ² -°C (Btu/hr-ft ² -°F)]	28,391 (5,000)
Tube plugging allowance (%)	1.0

For the electrically driven circulator, the flow rate during reactor shutdown is approximately 15% to avoid instability. At this flow rate, superheater tube sheet thermal shock may present a problem requiring extensive analysis and design development. A satisfactory superheater tube sheet design is being sought. RECS logic diagrams and computer inputs were prepared, but revisions and modifications are expected before it can be accurately used for steam generator development program planning and budgeting.

10.8. AUXILIARY CIRCULATOR, VALVE, AND SERVICE SYSTEM

A conceptual design of the electric drive auxiliary circulator has been completed (Fig. 10-11), a scoping study of the hydraulic drive system for the GCFR auxiliary circulator has been carried out, and the general arrangement of the auxiliary loop isolation valve has been determined (Fig. 10-12).

10.8.1. Auxiliary Circulator Design Requirements

As a result of the revised core auxiliary heat exchanger (CAHE) design, transient analysis of the residual heat removal (RHR) system during a DBDA indicates that adequate core cooling is achieved [with maximum cladding temperature equal to 1522 K (2280°F)] using two core auxiliary cooling system (CACS) loops, each having a 464.14-KW (622.4-hp) auxiliary circulator. The design parameters are given in Table 10-5. The lower power requirement of the auxiliary circulator compared with that reported in Ref. 10-10 is due to the lower rate of air ingress and the lower pressure drop in the CAHE. The difference in the air ingress rates of this and previous designs is accounted for by changes in the CACS start-up and transfer times, which in turn are related to inhalation of the containment gas. The lower air ingress rate results in a lower-density gas whose molecular weight is not appreciably different from that of helium.

10.8.1.1. Conceptual Design of Electrically Driven Auxiliary Circulator.

The rotational speed of the impeller was previously chosen to be 3600 rpm.

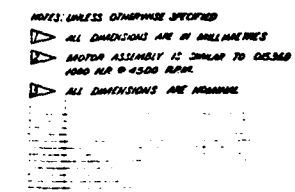


Fig. 10-11. Auxiliary circulator electric drive

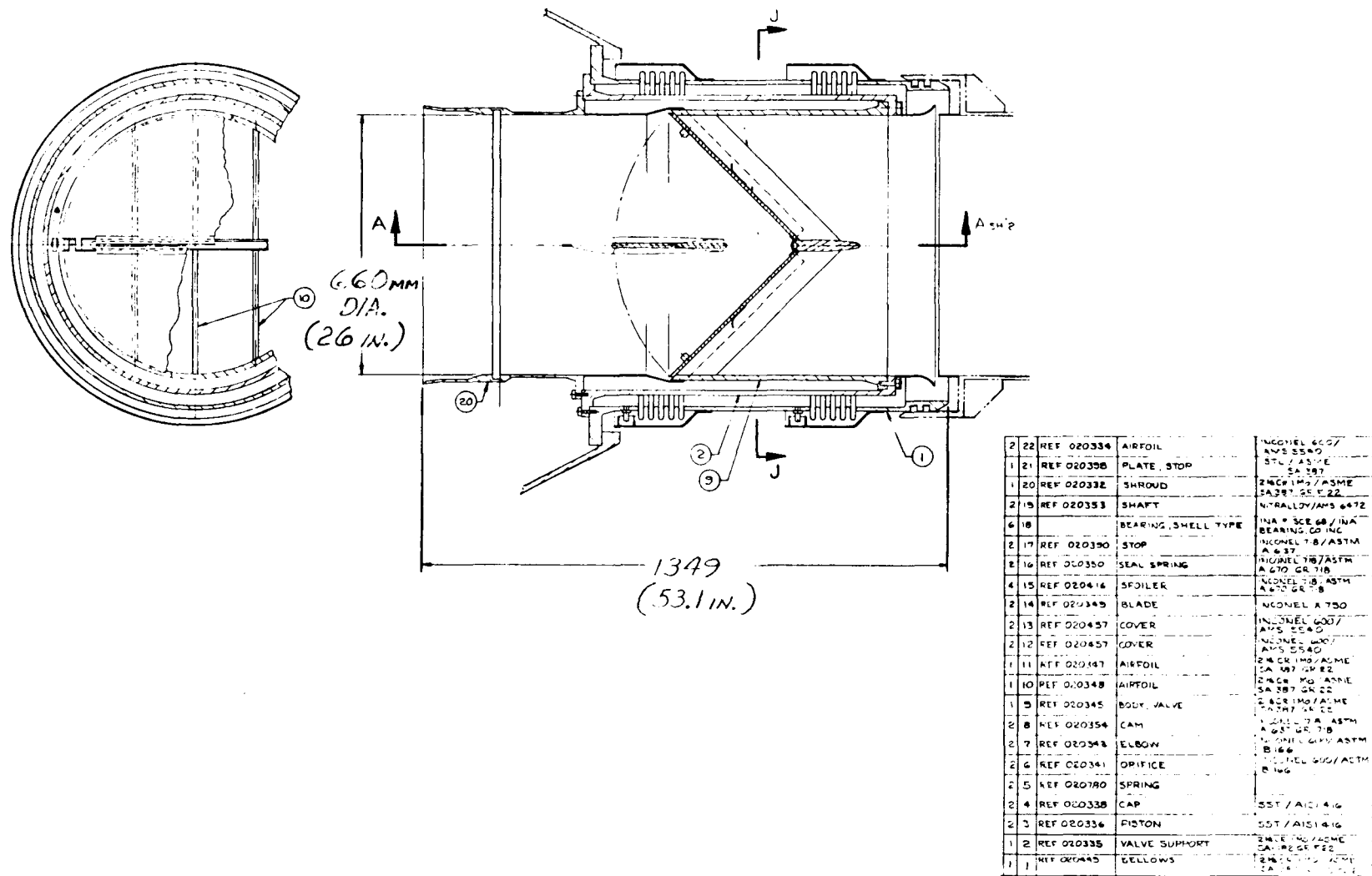


Fig. 10-12. General arrangement of auxiliary loop isolation valve (sheet 1)

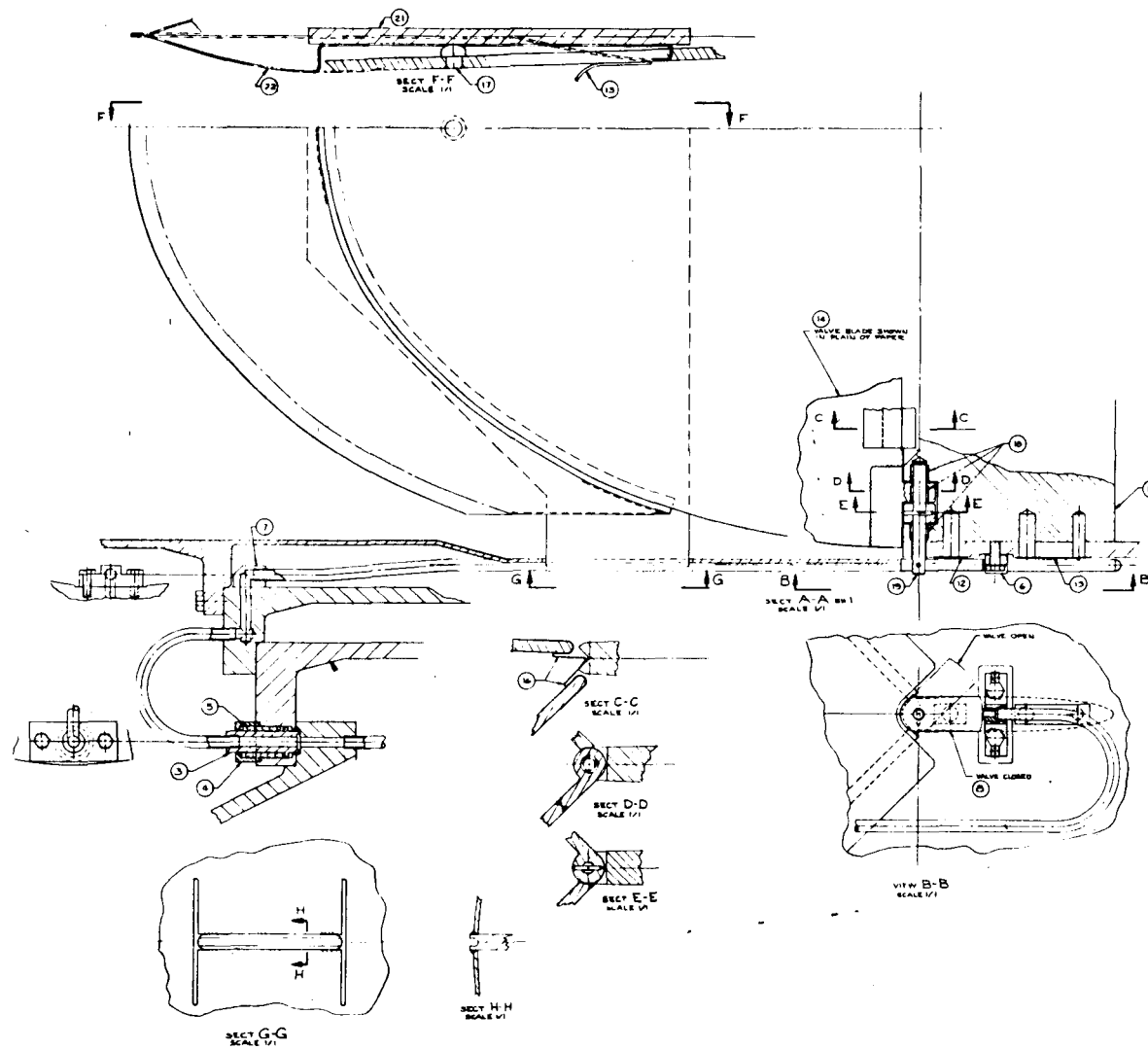


Fig. 10-12. General arrangement of auxiliary loop isolation valve (sheet 2)

TABLE 10-5
AUXILIARY CIRCULATOR REFERENCE DESIGN PARAMETERS

Parameter (one loop)	Pressurized Operation	DBDA Air Ingress
Mass flow rate [kg/s (lbm/s)]	9.57 (21.1)	5.41 (11.897)
Molecular weight (kg/mol)	4	4.06
Total inlet temperature [K (°R)]	535 (963)	512.8 (923)
Inlet pressure [MPa (psi)]	8.8253 (1280)	0.154 (22.4)
Discharge pressure [MPa (psi)]	8.8293 (1280.057)	0.164 (23.869)
Ratio of specific heats	5/3	1.6656

At that speed, the cavity diameter was 3.6576 m (12 ft) as a result of the large diameter of the impeller [1.62 m (63.94 in.)]. A 4500-rpm impeller to reduce the size of the PCRV cavity is being studied. The resulting dimensions are shown in Table 10-6. The new design point, when plotted on a typical Balje (or N_s - D_s) diagram, lies in the high-efficiency island of 80%, achievable with a centrifugal impeller.

It is desirable that all operating conditions of the auxiliary circulator be in a speed range below first critical. If a 20% margin between the design speed and the first critical speed (5400 rpm) is allowed for, the resulting shaft dimensions are considerably larger than those employed in the HTGR auxiliary circulator. The rotor dynamics of the shaft system are being studied in greater detail, and the question of supercritical (or flexible) shaft operation is under consideration.

10.8.1.2. Conceptual Design of Hydraulically Driven Auxiliary Circulator.

A parametric study was carried out to select a design point system water flow rate which would give the best compromise between component size and efficiency and piping weight and pressure drop. The circulator was assumed to be a centrifugal compressor operating at 5000 rpm. Hydraulic drive systems for water flows between 0.0566 and 0.2832 m/s (2 and 10 ft³/s) were examined for a water flow velocity of 9.144 m/s (30 ft/s) in the pipes. Pump sizes and efficiencies, pipe size and weight, and turbine size and efficiency were calculated.

The results showed that the best combination of component and system characteristics appears to be obtained with a flow rate between 0.1699 and 0.2265 m/s (6 and 8 ft³/s). The pump head rise is between 427 and 533 m (1400 and 1750 ft), which can be provided by a two- or three-stage pump with impellers between 279.4 and 355.6 mm (11 and 14 in.) in diameter. The pump efficiency is ~80%. The system piping has a 152.4 to 177.8 mm (6 to 7 in.) inner diameter and weighs around 11.93 kg/m (8 lb/ft). The pressure drop per 30.48-m (100-ft) equivalent run is between 68.95 and 28.74 kPa (10 and 12 psi).

TABLE 10-6
AUXILIARY LOOP CIRCULATOR DESIGN DATA

	4500 rpm	5000 rpm
Type	Radial	Radial
Drive	Electric	Hydraulic
Design condition	DBDA	DBDA
Estimated efficiency (%)	80	80
Tip diameter d_2 [m (in.)]	1.3 (51.2)	1.17 (46.1)
Tip width b_2 [mm (in.)]	76.55 (3.0138)	79.3 (3.11)
Eye diameter d_{is} [m (in.)]	0.7556 (29.75)	0.73 (28.72)
Hub diameter d_{ih} [m (in.)]	0.3778 (14.87)	0.365 (14.36)
Adiabatic head H_{ad} [kJ/kg (Btu/lbm)]	68.8 (29.58)	68.8 (29.58)
Volumetric flow Q [m^3/s (ft^3/s)]	36.66 (1294.7)	36.66 (1294.7)
Specific speed $N_s \left(\frac{ft \cdot lbm^{\frac{3}{4}}}{min \cdot s^{\frac{1}{2}} \cdot lb_f^{\frac{3}{4}}} \right)$	86.64	96.26
Specific diameter $D_s \left(\frac{s^{\frac{1}{2}} \cdot lb_f^{\frac{1}{4}}}{ft^{\frac{1}{4}} \cdot lbm^{\frac{1}{4}}} \right)$	1.46	1.32

The hydraulic turbines for the system in this flow range have single-stage radial inflow or Francis-type runners with a tip diameter of ~ 203.2 mm (8 in.). The tip widths are ~ 12.7 mm (1/2 in.), and the exit diameters are between 101.6 and 127 mm (4 and 5 in.). The turbine efficiencies are between 80% and 85%. The exact dimensions and system parameters depend on the exact value of the flow. It is recommended that a system with a flow rate of 0.1699 and 0.2265 m/s (6 and 8 ft³/s) with a piping water velocity of 9.144 m/s (30 ft/s) be selected.

10.8.1.3. Auxiliary Loop Isolation Valve. A layout of the proposed loop isolation valve design for the auxiliary cooling loop has been completed. The concept is identical to that used on large HTGR auxiliary cooling loops with the modifications necessary for the GCFR requirements. The design consists of a flow-actuated two-blade flapper valve located upstream of the auxiliary circulator inlet. Each blade can be moved independently of the other and is pivoted in rolling element bearings and supported in the closed position by a grid of three struts. A double bellows configuration on the valve support housing is used to overcome manufacturing tolerances and eliminate thermal expansion and alignment problems. Each valve blade has a fluidic position indication device, and high-pressure helium is blown through a calibrated jet onto a cam which is fixed to the valve blade shaft. The gap between the jet exit and the cam is dependent on the valve position; the gas flow will be used for valve position indication.

10.8.2. Core Auxiliary Heat Exchanger

The purpose of this task is to develop a CAHE which will meet the performance, safety, and reliability criteria of the CACS. A bottom-fed CAHE arrangement is being considered, especially since it has the capability of in-service inspection. With alloy 800H tubing material, in-service inspection of the tube bundle can be achieved by using the eddy current method. A detailed technique is being developed for the HTGR and Fort St. Vrain steam generators. The major concern centers around the primary support weld inspection. Two designs, a bayonet and a helically coiled

CAHE, present two completely different support structures. The bayonet CAHE is supported primarily by the tube sheets, which are readily inspectable. The helical bundle support is difficult to examine visually, but could be designed to provide redundant support to meet the code requirements. The selection of a reference design shall be heavily influenced by its capability for in-service inspection. General layouts were made of both configurations, and a comparison study is in progress. No major CAHE design activity is planned before the RHR criterion is established.

10.9. HELIUM PROCESSING COMPONENTS

A study to establish the performance and design requirements for the HPS has been completed. The conclusions of this study are expected to result in reduced equipment requirements and costs and simplified plant operation. A design criteria document has been prepared for the helium purification system (Ref. 10-12). RECS schedules have been prepared for the HPS and PES.

10.10. CONTROL AND ELECTRICAL COMPONENTS

10.10.1. Circulator Electric Drive Motor and Controller

A limited authorization to perform one month's work on the circulator electric drive and controller study was issued to Westinghouse. The purpose of the study is to confirm the feasibility of an electric motor drive for the main helium circulator. The entire study is expected to continue for six months.

10.10.2. Plant Protection System

A study to determine the possibility of applying the CACS loads to the required 60-s start-up of the auxiliary cooling system electric drive under varying conditions of plant electrical availability indicates that it is feasible. Further work will be required when the plant transients are better defined.

Preliminary work to determine the best approach to ensuring operation of the main helium circulator drive during RHR has been completed. The results indicate the advisability of coupling an alternate drive (such as a pony motor) to the main circulator drive shaft and supplying drive with a Class IE power source.

10.10.3. RECS Schedules

RECS schedules pertaining to control and electrical component items have been completed.

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11. CIRCULATOR TEST FACILITY (189a No. 00586)

The objective of this task is to develop a facility for qualification testing of the GCFR main helium circulator. The scope of this task involves (1) evaluation of alternative test facility concepts in terms of technical feasibility and cost, (2) identification of the most promising test facility concept, (3) an architect/engineer conceptual design, and (4) final design, construction, and checkout of the facility. The available FY-78 funding does not permit the conceptual design of separate steam and electric test facility concepts. Consequently, the steam concept has been deleted, and the test facility will be designed for an electric-motor-driven circulator. In the event that the alternate main circulator concept studies being conducted retain the steam-driven circulator, a steam loop can be added later.

The electric circulator test facility concept consists of an electrical supply and distribution system connected to an electric-motor-driven radial compressor. Electrical power is taken from a local utility grid via a transformer and furnished to the circulator drive motor by means of a solid-state motor controller. The circulator compressor is mounted in a large, heavy-walled pressure vessel with an internal configuration which provides a helium flow loop. Valves are provided for restricting the helium flow generated by the compressor to simulate reactor primary coolant loop pressure drop. A heat exchanger is also included to reject the heat generated by the compressor.

During the last quarter, schedules and specifications were prepared which coordinated the circulator test facility conceptual design with the electric motor and controller study. A statement of work describing the circulator test facility and a schedule for the architect/engineer conceptual design was prepared and distributed to several architect/engineering firms.

During this quarter the Ralph M. Parsons Company was selected to perform the conceptual design task. Conceptual design activities are under way. Architect/engineer activities to date include the preparation of facility layouts and helium loop test vessel designs. Efforts at GA have centered on supplying the architect/engineer with needed technical information and review of layouts and vessel designs. The total estimated cost for this project calculated by GA is \$19,500,000 in 1978 dollars. This estimate was done without the benefit of input from the architect/engineer and will be updated when the conceptual design is complete.

12. PLANT DYNAMICS (189a No. 00638)

12.1. CONTROL SYSTEMS

A second version of the open-loop plant dynamic simulation was developed. This version includes an electric-motor-driven helium circulator in place of the steam-turbine-driven circulator in the first version. A new helium circulator/driver model was developed, the steam generator output flow was directly connected to the main steam header, and new simulation input data consistent with this particular plant configuration were generated. This second version was developed because of the increased emphasis placed on the electrically driven circulator configuration and the significantly different control system problems inherent in the two plant configurations.

Some problems appeared in the steam generator model when the new configuration data were incorporated. These problems, which were traced to an eigenvalue which was too large to be compatible with the desired integration step size, were temporarily resolved by artificially increasing the inertia of the responsible state. The remaining eigenvalues were largely unaffected. Transient comparisons were made, and it was found that the magnitude but not the shape of the transient response to step changes in water/steam inputs was affected by the change in inertia. It was also noted that the initial response of the model to a step change in inlet flow evidenced characteristics associated with the seesaw problem. Since the large eigenvalue could be traced to the relatively small pressure drop across the evaporator, it was decided to reformulate the water/steam-side part of the model by assuming that the evaporator pressure drop was negligible. This effort proved successful in that the resulting model eliminated the high eigenvalue, and the transient response to step changes in inputs appeared reasonable. Comparison of the first and second versions showed good agreement for step changes in helium inlet temperature and flow rate.

Responses to step changes in inlet water temperature and flow and exit pressure were not satisfactory, however, and the investigation of this particular model is continuing.

12.2. SEISMIC ENGINEERING

There was no activity on this subtask during this quarter.

12.3. FLOW-INDUCED AND ACOUSTICALLY INDUCED VIBRATIONS

Efforts were continued to establish a more accurate power spectral distribution for high-shaft-power radial circulators. Reference 12-1 indicates that increases on the order of 25 to 30 dB in the low-frequency acoustic loads of the primary coolant boundary components could be expected if the axial flow steam-driven circulators were replaced by radial flow electric-motor-driven circulators. Further work revealed that the power spectral distribution used for the axial flow machine underestimated the acoustic energy at the low-frequency side of the spectrum and overestimated contributions at the high-frequency side, resulting in an incorrect comparison of the axial and radial circulators. A revised median power spectral distribution for the axial fan has been established and is presented in Fig. 12-1. Because of this revision, the difference between the expected acoustic loads on the primary coolant boundary components due to only broadband noise were reduced. Table 12-1 lists the expected sound pressure levels for the total primary coolant excluding the core and shows an 8-dB increase for the radial circulator for the lowest band, decreasing to about 3 dB for the octave band with a center frequency of 250 Hz. Table 12-2 presents the total expected sound pressure levels in the various primary coolant volumes caused by the one-stage axial circulators (broadband noise only). The scoping study (Ref. 12-2) has been revised accordingly. Note that the axial circulator power spectral distribution is a median distribution, which means that there is a 50% chance that these values will be exceeded. The probability that the final expected sound pressure levels of Table 12-2 will be exceeded is less than 50% because of the conservatism in the analysis. It has been assumed for both circulators that 15% of the

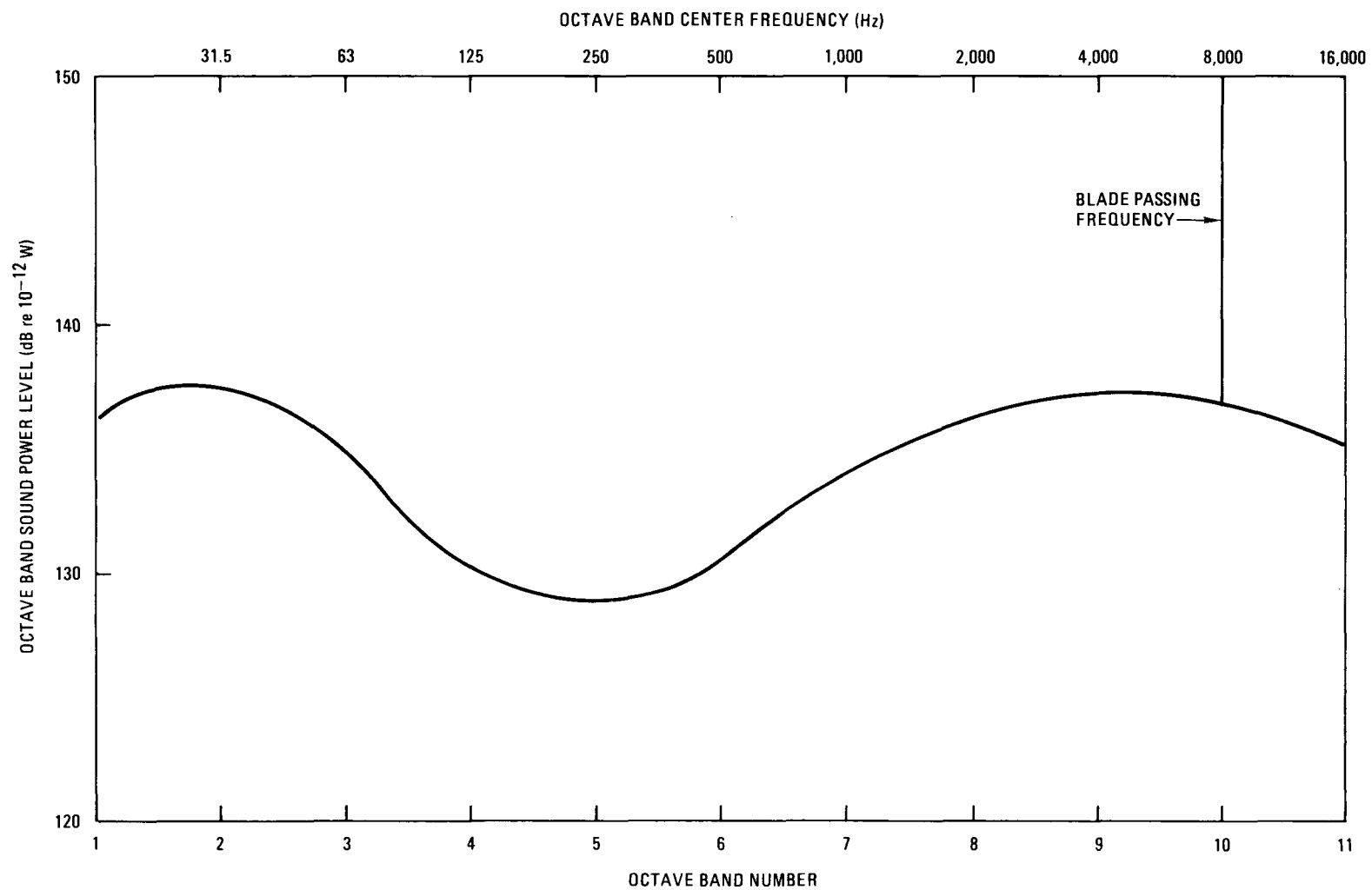


Fig. 12-1. Main helium circulator power spectrum (one-stage axial fan)

TABLE 12-1
 EXPECTED SOUND PRESSURE LEVEL V_{tot} (BROADBAND ONLY)

Octave Band No.	dB re $2 \cdot 10^{-5}$ Pa	
	Axial Circulator	Radial Circulator
1	147.6	154.5
2	148.8	154.3
3	145.9	150.7
4	141.2	147.0
5	139.6	142.8
6	141.1	138.4
7	143.6	133.9
8	145.4	129.5
9	146.2	121.8
10	145.1	111.0
11	142.3	--
Total	155.5	158.6

TABLE 12-2
EXPECTED SOUND PRESSURE LEVELS (BROADBAND ONLY)

Octave Band No.	dB re 2×10^{-5} Pa				
	Entire Primary Coolant System	Steam Generator Exit Annulus	Main Circulator Exit Plena	Cold Gas Return Duct	Lower Core Cavity
1	147.6	150.0	147.6	147.6	147.6
2	148.8	151.3	148.8	148.8	148.8
3	145.9	148.4	145.9	145.9	145.9
4	141.2	143.8	141.2	141.2	141.7
5	139.6	142.3	139.6	139.6	139.9
6	141.1	144.0	141.1	141.1	141.2
7	143.6	146.7	143.6	143.6	144.5
8	145.4	148.7	145.4	145.4	150.7
9	146.2	150.0	146.2	146.4	148.1
10	145.1	149.2	156.4	145.4	145.8
11	142.3	147.0	154.5	142.7	142.6
Total	155.5	158.6	160.1	155.6	156.8

overall acoustic source strength is injected at the blade passing frequency or its harmonics. A conservative scoping analysis of a typical HTGR thermal barrier cover plate vibrating at its fourth free vibration mode at an undamped frequency of 40 Hz showed that for both the axial and radial circulators, the acoustically induced stresses in the cover plate exceed the maximum allowable stress for 10^6 cycles, which is equal to 90 MPa. Further work is required to improve the analytical methods and thermal barrier cover plate design to ensure that the plates can withstand the expected acoustic loads in the GCFR primary coolant.

The conceptual design of the GCFR main helium circulator test facility was continued. In view of the lack of acoustic data for radial circulators in the range 2 to 20 MW and the urgent need for especially accurate low-frequency data, serious consideration is being given to modifying the conceptual design of the circulator test facility in such a way that the inlet and outlet plena of the circulator and adjacent ducting will be prototypical. The facility as presently designed will not provide low-frequency acoustic data.

The flow-induced vibration scoping study (Ref. 12-3) has shown that the in-plane and out-of-plane bending frequencies of the outermost steam generator coils are in the range 20 to 22 Hz and the critical velocity for the onset of whirling can be expected around a gas velocity of ~ 35 m/s (for the reference design steam generators with six radial support plates). Since the average interstitial helium velocity is ~ 30 m/s, it was concluded that further experimental and analytical evaluations of whirling phenomena in the steam generator units were required. Therefore, it has been requested that the heat exchanger coil of the main helium circulator test facility be used to perform dynamic tests. These tests would serve to verify theoretical models and provide experimental data on damping, midspan amplitudes, etc.

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- 12-3. Halvers, L. J., "Flow-Induced Vibrations in the 300-MW(e) Gas-Cooled Fast Breeder Reactor Demonstration Plant: A Scoping Study," DOE Report GA-A14700, General Atomic Company, to be published.

13. REACTOR SAFETY, ENVIRONMENT, AND RISK ANALYSIS (189a No. 00589)

The purpose of this task is to investigate and quantify GCFR safety characteristics. A liaison and coordination subtask integrates the DOE-sponsored GCFR safety work at GA and the national laboratories into a national GCFR safety program which is responsive to the need for GCFR safety research. A GCFR safety program plan is being developed to define the safety research needed for the demonstration plant and the longer-term GCFR commercialization program. Safety research at GA includes probabilistic accident analysis, accident consequence analysis, radiological and environmental analyses, and postaccident fuel containment (PAFC) analyses.

Logical probabilistic methods are employed to determine the probabilities associated with various accident initiation and progression sequences and to identify potential design modifications which would help reduce risks. A methodology for integrating reliability considerations into the GCFR engineering effort at the system, subsystem, and component levels is being developed for trial use on a selected system, with the objective of determining the optimal use of reliability engineering methods in the GCFR. The thermal behavior of the fuel assembly under conditions of loss of shutdown heat removal is being analyzed to determine the heat-up and melting sequence of the cladding, duct walls, and fuel, because duct wall melting has been identified as an important phenomenon influencing the accident sequence. PAFC analyses are being performed to assess the capability of the current design and to identify potential modifications which could improve the molten fuel containment capability.

13.1. REACTOR SAFETY PROGRAM COORDINATION

The GCFR safety program plan has been completed, and the safety program is now organized into five tasks, as shown in Fig. 13-1. The task

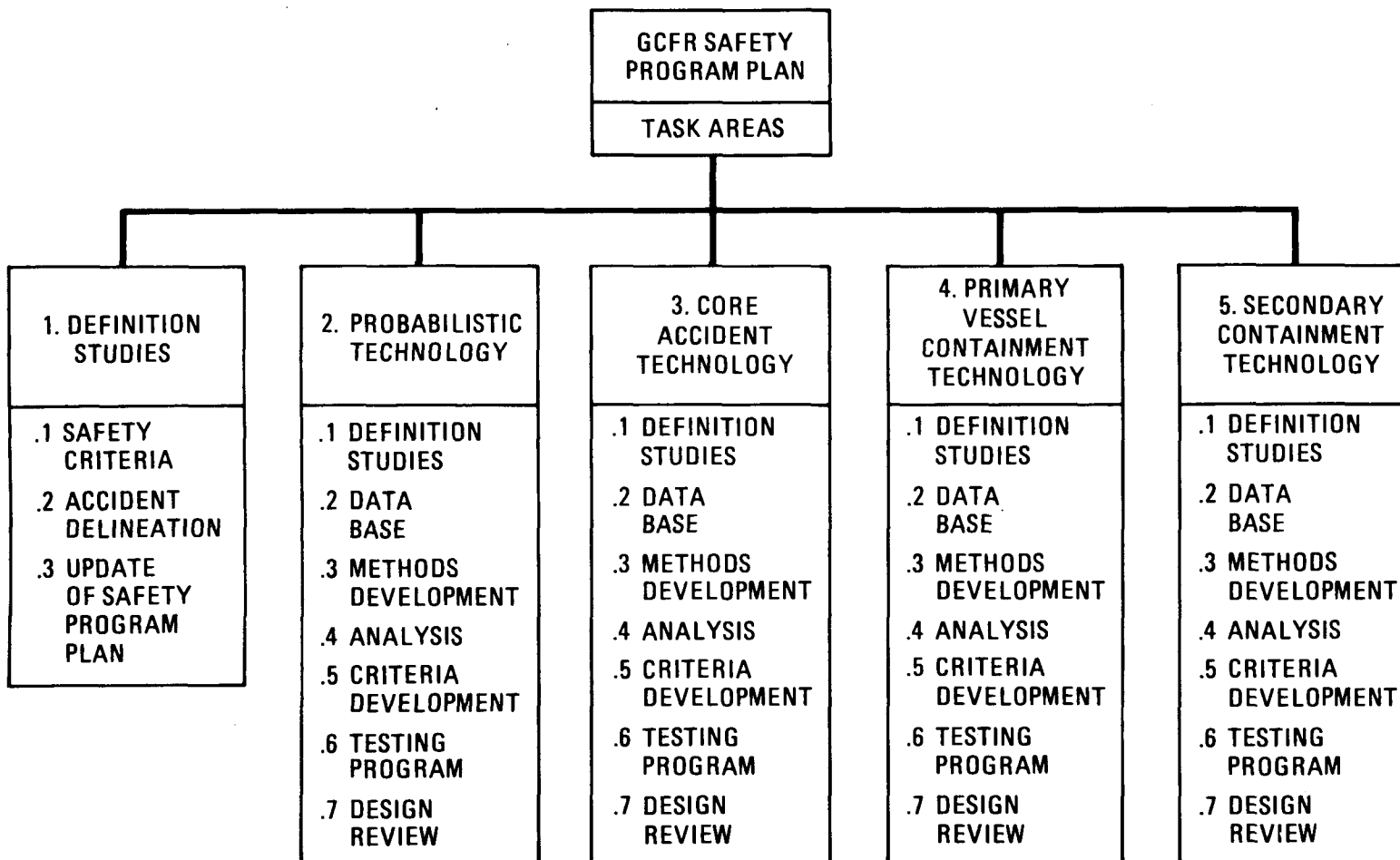


Fig. 13-1. Safety program task breakdown within task areas

scopes have been defined, and work is in progress to develop individual task schedules and manpower projections. Over the past 18 months, significant work has been completed on probabilistic safety analysis and post-accident fuel containment. The major conclusions of this work are

1. A reliability target of 10^{-6} /yr for residual heat removal (RHR) can be supported by the main loop and core auxiliary cooling system (CACS) loop hardware; however, the current RHR support systems do not support this conclusion.
2. Reliance on the main circulator inertia coast-down function to bridge the CACS start-up gap appears to be consistent with current Nuclear Regulatory Commission (NRC) practice. However, the reliability data derived from jet engines and stationary power turbines do not support a reliability target of 10^{-6} /yr for this function.
3. Simple qualitative and quantitative analysis methods exist which can be used by design engineers to enhance design reliability.
4. A significant data base for generic gas-cooled reactor equipment which is comparable to the reactor safety study (Ref. 13-1) data base for light water reactors (LWRs) is available from European operating experience with gas-cooled reactors and has in large part been implemented in the gas-cooled reactor reliability data bank.
5. In-vessel PAFC is technically feasible in that heat loads can be handled by engineered systems and geometric constraints permit configurations which are subcritical. Several promising design concepts have been identified.
6. An in-vessel molten core retention scheme has been shown to have the greatest potential for mitigating the risk from a core melt-down accident.

7. The primary function of a core retention system as a risk-mitigating system is to prevent failure of the containment in the upward direction. The secondary function is to prevent melt-through in the downward direction. All molten core retention schemes outside the PCRV liner increase the risk from upward containment failure.

13.2. PROBABILISTIC ACCIDENT RISK ANALYSIS

Accident initiation and progression analysis (AIPA) techniques initially developed in FY-74 (Ref. 13-2) are being applied to the probabilistic analysis of potential accident sequences leading to low-probability, high-consequence outcomes. The consequences of these sequences are also under study at ANL and GA (under other subtasks). The objective of this work is to assess the risks of these accident chains in the GCFR.

13.2.1. Comparison of Reliabilities of Alternate Main Circulator Configurations

Comparative reliability analyses have been updated for the six GCFR main circulator drive configurations listed below.

1. Steam turbine circulator inside PCRV (reference configuration).
2. Steam turbine circulator outside PCRV.
3. Electric drive outside PCRV.
4. Electric drive with pony motor outside PCRV.
5. Electric drive inside PCRV.
6. Electric drive with pony motor inside PCRV.

These analyses indicated that there are no significant identifiable differences for the six systems with respect to main loop RHR reliability or plant availability. The analysis considered the main circulator drive system and its control and support systems. The impact on plant availability was considered using the basic components utilized during normal operation.

No significant differences could be established in the total system unavailability within the confidence range for the data used.

The reliability analysis for main loop RHR is summarized in Table 13-1. The estimated main circulator failure probability per year includes the circulator and its support components and excludes other main loop RHR equipment and peripheral support systems. These values do not constitute a large fraction of the overall main loop RHR failure probability per year. Thus, the circulator configurations are not the dominating contributor to main loop RHR failure; instead, the peripheral equipment is the dominant contributor to main loop RHR failure probability.

No significant reliability advantages inherent in any of the six configurations can be identified for main loop RHR, and it is concluded that each of the six systems can provide acceptable reliability with respect to main loop reliability considerations. The choice of circulator drive system must therefore be based on other considerations such as performance, state of the art, functional requirements, and diversity.

13.2.2. Comparison of Reliabilities of Alternate CACS Standby Operating Modes

Comparative reliability analyses for three alternate CACS readiness modes were completed. Alternate CACS standby modes are being investigated to evaluate means of reducing dependence on only main loop RHR during the CACS start-up time period following a reactor scram. The analyses considered the current CACS design with the following standby modes:

1. All major active components on standby (reference design).
2. An idling auxiliary circulator with all other active components on standby.
3. All major active components running or idling.

TABLE 13-1
RESULTS OF COMPARATIVE ANALYSES OF RHR RELIABILITY OF VARIOUS
GCFR CIRCULATOR CONFIGURATIONS^(a)

Main Circulator Configuration	Failure Probability Per Yr	
	Main Circulator Only	Total Main Loops
Turbine circulator inside PCRV		
Independent	2.4×10^{-4}	4.0×10^{-1}
Common cause (b)	7.8×10^{-3}	4.5×10^{-1}
Turbine circulator outside PCRV		
Independent	2.9×10^{-4}	4.0×10^{-1}
Common cause	8.9×10^{-3}	4.5×10^{-1}
Electric circulator outside PCRV		
Independent	1.0×10^{-7}	3.0×10^{-1}
Common cause	3.2×10^{-3}	3.2×10^{-1}
Electric circulator with pony motor outside PCRV		
Independent	1.0×10^{-6}	3.0×10^{-1}
Common cause	1.1×10^{-3}	3.0×10^{-1}
Electric circulator inside PCRV		
Independent	7.0×10^{-6}	3.0×10^{-1}
Common cause	2.1×10^{-3}	3.2×10^{-1}
Electric circulator with pony motor inside PCRV		
Independent	1.0×10^{-6}	3.0×10^{-1}
Common cause	5.5×10^{-4}	3.2×10^{-1}

(a) Three scrams plus 10 controlled shutdowns per yr with an average of 135 hr per outage (80% plant availability) are assumed.

(b) Common cause failure fractions between redundant loops are assumed to be 0.1 and 0.01 for standby and operating equipment, respectively.

The conclusions of the analyses, assuming independent loops, were as follows:

1. For pressurized RHR
 - a. The standby reference design indicated a failure probability of 1×10^{-4} per demand.
 - b. With only the auxiliary circulator idling, there is no significant improvement in RHR reliability.
 - c. With an idling or running CACS, an RHR reliability improvement of about one-half order of magnitude can be expected.
2. For depressurized RHR
 - a. For short-term CACS operation (less than 1 week), the results are similar to those for the pressurized PCRV.
 - b. For long-term CACS operation (1 month), there is no significant reliability improvement because the running failure probabilities become more significant than the initial start failure probabilities.

The analyses indicated a less significant improvement in RHR reliability when generic common cause failure fractions were assumed. It has been concluded that all CACS standby modes considered can meet the reliability target of 1×10^{-4} /demand if the loops are considered independent. However, the analyses did not include the CACS control system. This will have to be done when the system becomes available. No significant differences were noted in plant availability; the indicated differences were less than a factor of 1.5, which is within the data base uncertainty range.

The estimated CACS RHR unreliability and plant unavailability are shown in Table 3-2. Three variations of the idling CACS system were analyzed, and the results indicate little difference among the three alternatives.

TABLE 13-2
GCFR CACS RELIABILITY AND PLANT UNAVAILABILITY ESTIMATES FOR VARIOUS STANDBY OPERATING MODES
(EXCLUDING ELECTRICAL POWER SUPPLIES AND CONTROLS)

Operating Mode	Unreliability Q								Total Plant Unavailability ^(d)	
	Pressurized PCRV ^(a)				Depressurized PCRV ^(b)					
	Independent		Common Cause ^(c)		Independent		Common Cause			
	Q	Δ ^(e)	Q	Δ	Q	Δ	Q	Δ		
Standby, reference design	1.3E-4 ^(f)	--	7.8E-4	--	4.1E-4	--	3.1E-3	--	38	--
Idling auxiliary circulator only	1.0E-4	1.3	7.0E-4	1.1	3.4E-4	1.2	2.9E-3	1.1	44	1.2
Idling CACS ^(g)	2.9E-5	4.5	4.3E-4	1.8	2.6E-4	1.6	2.7E-3	1.1	58	1.5
Idling CACS ^(h)	3.5E-5	3.7	4.6E-4	1.7	2.5E-4	1.6	2.7E-3	1.1	56	1.5
Running CACS ⁽ⁱ⁾	2.4E-5	5.4	4.5E-4	1.7	2.5E-4	1.6	2.7E-3	1.1	58	1.5

(a) ≥ 2 of 3 CACS loops (20 min) + ≥ 1 of 3 loops for 46 hr; 46-hr mean time to restore main loop cooling system; repairs outside containment only.

(b) ≥ 2 of 3 CACS loops (20 min) + ≥ 1 of 3 loops for 720 hr (1 month); no main loop cooling system.

(c) Common cause fraction of 0.1 for standby items and 0.01 for running items assumed; CACS is used only when main loop cooling system is not available.

(d) Assuming technical specifications allow 24 hr of operation with one CACS loop down.

(e) Δ = ratio of standby reference design CACS reliability to idling or running CACS reliability.

(f) $1.0E-4 = 1.0 \times 10^{-4}$.

(g) Idling auxiliary circulator, low-load (bypass) core auxiliary cooling water system pump, low-load (stalled) fans, loop isolation valves and louvers normally closed.

(h) Idling auxiliary circulator, idling (two-speed) core auxiliary cooling water system pump, low-load (stalled) fans, loop isolation valves and louvers normally closed.

(i) Auxiliary circulator running at pressurized conditions, core auxiliary cooling water system water pump running, low-load (stalled) fans, loop isolation valves and louvers normally closed.

The component failure rates employed in this study were estimated as described in Section 13.6.

13.3. ACCIDENT CONSEQUENCE ANALYSIS

13.3.1. Introduction

This subtask investigates the consequences of low-probability accident sequences leading to core damage. Its objective is to determine the capability of the core, PCRV, and containment barriers to mitigate the release of radioactive material from the containment. The first step is to investigate the expected behavior of the GCFR core. Particular emphasis is given to the protected loss of flow (PLOF) accident, which has been shown to be the dominant contributor to the probability of a loss of coolable core geometry. Analyses of unprotected accidents are currently being emphasized at ANL.

Two analyses of the PLOF accident sequence were performed during this quarter. The first analysis indicates that stainless steel blockages may form between duct walls as a consequence of molten steel spillover from a subassembly. The second concludes that relocation of all core cladding to the lower axial blanket and compaction of the outer two fuel rod rings of each subassembly are insufficient to cause recriticality at the core level with all control rods inserted. Extensive changes to the BOXRAD (Ref. 13-2) computer program have been initiated in preparation for future, more detailed, whole-core heat-up analyses during a PLOF accident. The first phase of these modifications allows a loss of flow transient to be quantified from full-power steady-state conditions through control rod insertion and circulator coast-down and cladding and duct wall melting to fuel melting.

13.3.2. Molten Steel Freezing Characteristics in the Interduct Spacing

During the course of a PLOF, the cladding and duct wall melt (Ref. 13-3). The molten cladding and duct wall steel flow toward the lower

axial blanket, where they refreeze and block the lower axial blanket coolant channels to a depth of 10 to 20 cm below the core bottom (Ref. 13-4). Before remelting of this blockage is possible via contact with molten fuel, continued duct wall melting creates a molten steel puddle within the core region (Ref. 13-5). The puddle backfills the coolant channels until it reaches the elevation of the melted-through opening in the duct wall, and it becomes a source of downward steel motion along the duct wall surface within the interduct spacing. For the purposes of this study, the motion was assumed to be gravity-driven flow which may be in the form of a slug or film. The velocity of the flow was assumed to be limited by either the mass spillover rate or frictional shear forces. The latter becomes the velocity limitation only for a very thin film.

Since the molten steel is superheated at the spillover elevation, it loses heat to the duct wall as it flows toward the lower axial blanket. This analysis addressed the potential of the flowing steel to refreeze and block the interduct spacing. Such an assessment is of interest in determining if fuel subassemblies will fall away when circumferential duct wall melt-through is complete. Duct fallaway is a pivotal factor in assessing recriticality during a PLOF accident.

A computer program called STEFINS (steel freezing in the interduct spacing) has been developed for determination of the flow and the freezing characteristics of molten steel within the interduct spacing during a PLOF. The program solves a series of differential and algebraic equations which describe the heat transfer from flowing molten steel through a time-varying refrozen steel layer and the duct wall to the rod bundle. The molten steel may flow as a gravity-driven slug or film, and the flow may be mass flow rate limited or friction limited. This program has been used to determine the time and location of molten stainless steel freezing in the interduct spacing assuming that flow rate is limited by the rate at which molten steel spills from the subassembly. The heat transfer problem reduces to that represented by a flow along a cold plane wall of finite thickness.

The solidification process is initiated when saturated or superheated flowing stainless steel contacts the duct wall at a position which is cold enough to maintain the instantaneous liquid-to-solid interface temperature below the steel melting point. As the thickness of the solidified layer on the wall increases, heat is transferred by convection from the flowing liquid to the liquid-solid interface. Heat is removed from the interface through the solidified layer and duct wall by transient conduction, and sensible heat is stored in the solid regions; latent heat is stored at the interface. Solidification proceeds as long as the conduction heat flux away from the interface exceeds that to the interface from the molten stainless steel. An equilibrium will eventually be reached, at which time the heat flux across the interface will be continuous and the solidified layer will no longer grow. If this equilibrium layer is larger than one-half the interduct spacing and the spacing is indeed symmetric, then a blockage will eventually occur. Consequently, it becomes relevant to consider whether (1) the spillover mass inventory is sufficient to allow a blockage to occur, or (2) the entire available molten stainless steel inventory can drain out of the interduct spacing prior to blockage. If the thickness of the equilibrium solidified layer is less than one-half the interduct gap, blockage will never occur.

For all cases considered to be physically relevant to a PLOF in a GCFR, the equilibrium solidified layer thickness is larger than half the interduct gap width. Complete blockage will occur if sufficient molten steel inventory is present. It will not occur if all the steel can drain out of the subassembly prior to the solidified layer thickness reaching half the interduct gap width. About 3 kg of molten steel per duct face are available for spillover. A mass flow rate of 1520 gm/s would allow drainage of the entire inventory prior to blockage. This is a factor of two higher than the spillover rate predicted by extrapolation from previous analyses of the GCFR design (Refs. 13-2, 13-3). Therefore, blockage of the interduct spacing is considered likely with the current design.

If it is possible for blockage to occur, determination of the most likely location for the initial blockage formation becomes important.

Calculations predict that molten steel freezing will not occur until the molten flow front reaches the lower axial blanket. Table 13-3 shows that a complete blockage may occur over virtually the entire axial extent of the duct wall located below the core bottom (i.e., below the core - lower blanket interface). This blockage will occur first within 3.5 cm of the core bottom in the lower axial blanket, because the time required for axial progression of the flow front more than compensates for the effect of reduced wall temperatures at lower elevations. Since the molten steel is not expected to freeze far below the core bottom, the blockage depth may be limited to a few centimeters. This result is particularly sensitive to the assumed mass flow rate, as shown in Tables 13-4 and 13-5.

In Tables 13-3 through 13-5, film flow is defined as the flow of a layer of molten steel with a flow area which is less than that of the interduct spacing. The tables quantify the results in terms of time to blockage for slug and film flow. Freezing time is defined as the increment from the time the slug or film front reaches the axial location of interest to the time the blockage is complete (i.e., spans the interduct gap). Flowing time equals freezing time plus the additional increment from the time of spillover to the time the flow front reaches the axial location of interest. Other parameters which affect the results are the temperature of the spilling molten steel, the duct wall surface temperature adjacent to the rod bundle, the flowing molten steel film thickness, and the flow regime. Experiments will be required to resolve the substantial uncertainty associated with the flow regime.

13.3.3. Partially Crumbled Core Recriticality Evaluation

Previous analyses (Ref. 13-6) related to bowing and stressing of declad fuel columns during a PLOF accident have identified the possibility of fuel column failure prior to incipient fuel melting. Because of the axial burnup dependence, axially nonuniform heat transfer occurs from the outer two rings of declad fuel columns to the duct wall of each subassembly. The bowing-induced stresses may be sufficient to fracture the outer two

TABLE 13-3
BLOCKAGE TIME AS A FUNCTION OF AXIAL LOCATION IN THE LOWER BLANKET^(a)

Distance ^(b) (cm)	Time to Blockage (s)			
	Flowing Time		Freezing Time	
	Slug Flow	Film Flow	Slug Flow	Film Flow
3.53	12.3	4.5	4.3	2.8
17.65	19.1	6.0	2.8	2.8
31.77	27.1	7.5	2.6	2.6
77.70	53.6	12.8	2.5	2.6

(a) The temperatures were generated by BOXRAD runs: molten steel spillover temperature = 2244 K; spillover height = 10.59 cm above core bottom; mass flow rate = 760 g/s.

(b) Measured downward from core - lower axial blanket interface; 77.70 cm is the distance to the bottom of the subassembly duct wall.

TABLE 13-4
MASS FLOW RATE VARIATION^(a)

Mass Flow Rate (g/s)	Time to Blockage (s)				Film Thickness ^(b) (cm)
	Flowing Time		Freezing Time		
	Slug Flow	Film Flow	Slug Flow	Film Flow	
76	84.4	10.3	2.8	2.8	0.039
760	12.3	4.5	4.3	2.8	0.085
1520	20.2	6.6	16.2	5.6	0.104
2280	(c)	(c)	(c)	(c)	0.114
3040	(c)	(c)	(c)	(c)	0.122

(a) Molten steel spillover temperature = 2244 K; spillover height = 10.59 cm above core bottom; calculations performed 3.53 cm below core bottom.

(b) Film thickness corresponds to given mass flow rate and spread width of 5 cm.

(c) An equilibrium solidified layer thickness was obtained for these cases. The thickness of the layer was less than one-half the width of the interduct gap. The equilibrium quantities are shown in Table 13-5.

TABLE 13-5
EQUILIBRIUM QUANTITIES

Mass Flow Rate (g/s)	Flow Mode	Case Termination Time (s)	Solidified Layer Thickness (cm) (a)	Wall Temperature (K)
2280	Slug	124	0.268	1537
2280	Film	48	0.204	1456
3040	Slug	118	0.236	1528
3040	Film	57	0.148	1483

(a) Interduct gap width = 0.65 cm.

rings of fuel columns in each subassembly and allow them to compact upon the previously formed steel blockages in the lower axial blanket (Ref. 13-4). This fuel will tend to displace the molten cladding in the lower core region which overlays the blockage. The configuration for one representative subassembly is illustrated in Fig. 13-2. The loss of core cladding coupled with the reduced leakage paths in the lower axial blanket, the spectral shift effect on control rod worth, and the fuel relocation reduce the reactor shutdown margin with all control and shutdown rods inserted.

Eigenvalues for two distorted core configurations based on Fig. 13-2 were compared with the undistorted core eigenvalue using an R-Z representation in the 2DB code (Ref. 13-7). The results are shown below.

Configuration	Reactivity Change from Normal Shutdown Condition with All Absorber Rods Inserted (\$)
Case A (1 row crumbled)*	4.1
Case B (2 rows crumbled)**	11.7

Of the \$11.7 increase in reactivity for case B, \$7.6 is due to the reduction in control rod worth. Even with fully depleted control rods, the reactor is still subcritical by at least \$3.8. Recriticality studies will continue as more configurations are established through investigation of the PLOF event sequence. It is anticipated that future neutronic studies will include corrections for transport effects, Doppler effects due to elevated fuel temperatures, and spectrum effects on control rod worth.

*Case A: all fuel rods in the core have lost cladding, which has relocated to the upper 20 cm of the lower axial blanket. The outer row of fuel pins in each subassembly above the core midplane is assumed to have crumbled, and the pellets have fallen to the bottom of the core. In addition, the corresponding blanket pellets have fallen on top of the fuel pellets, and all control and shutdown rods are fully inserted.

**Case B: The case A configuration is modified so that the outer two rows of fuel pins in each subassembly above the core midplane are assumed to have fallen.

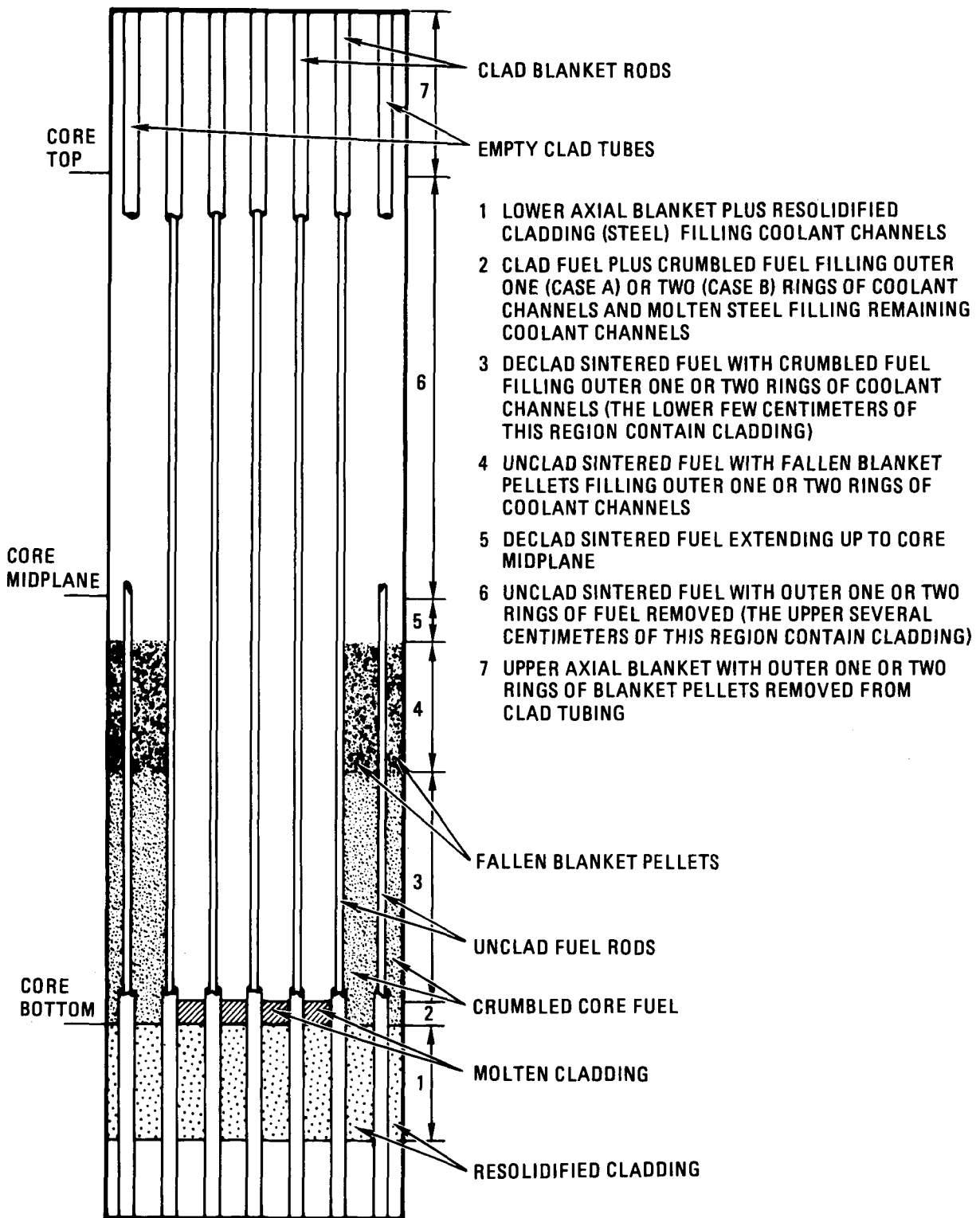


Fig. 13-2. Postulated subassembly transient configuration during a loss of decay heat removal accident

13.3.4. BOXRAD Modifications and Radial Node Structure Sensitivity Study

Extensive modifications to the BOXRAD code (Ref. 13-1) have been initiated. The changes to date include

1. Addition of forced convection heat transfer in the turbulent flow regime.
2. Updating of material property data to be consistent with Ref. 13-8.
3. Inclusion of a logic algorithm to distinguish between fuel, edge channel, and blanket regions.
4. Conversion of code to metric units.
5. Storage modifications to allow whole-core cases to be run within the normal capability of the UNIVAC 1110.

These changes will allow a consistent analysis of core heat-up during a PLOF from steady-state operation through fuel melting. To develop a consistent analytical procedure, a sensitivity study was undertaken to determine the node structure required to obtain acceptable accuracy in PLOF analyses. Sufficient accuracy is achieved if the numerical error does not exceed the variance due to uncertainties in material properties. In other words, the effect of changes in material properties must be easily discernible. The node structure was varied from a single fuel node to 25 fuel nodes per fuel rod. In each case the code was run for 60 s at full power and full coolant flow to reach a steady-state temperature distribution. At that point, the reactor was scrammed and the mass flow allowed to coast down to a Reynolds number of 2300. The flow was then set to zero, and the transient was continued to the onset of fuel melting. The results

of this study are presented in Fig. 13-3 and Table 13-6. The following conclusions have been drawn from this analysis:

1. Sufficient accuracy is obtained for steady-state conditions and during the first phase of coast-down with five radial fuel nodes per rod. A 6% error with respect to the exact solution is shown in Fig. 13-3 for steady-state conditions. The error due to a five-node structure is only 3% in the cladding response at the end of the circulator coast-down case. Furthermore, the five-node case is virtually indistinguishable from the exact solution by the time fuel melting occurs (Table 13-6). More than five radial fuel nodes would indeed be somewhat more accurate for determining the full-power steady-state initial conditions and during the first 10 s of coast-down. However, the extra nodes, and therefore the additional running time, cannot be justified for analysis of important phenomena during a PLOF transient. This is especially true in light of the core storage limitations of the UNIVAC 1110.
2. A decrease in the number of fuel nodes to three per rod allows an increase in the number of axial node locations possible in the whole-core analysis case to five. However, the three-node case overpredicts the steady-state fuel enthalpy by 16%, causing a 7% cladding temperature error at the end of coast-down and a corresponding error in the time to cladding melting. Comparable errors using five fuel nodes per rod are 7% at steady state, 3% at the end of coast-down, and 2.5% at incipient cladding melting.
3. The significant improvement in accuracy associated with five fuel nodes per rod over that of three nodes per rod for the entire transient (steady state through fuel melting) eliminates the need for mesh collapse, as shown by comparing case 2 with case 1 in Table 13-6.

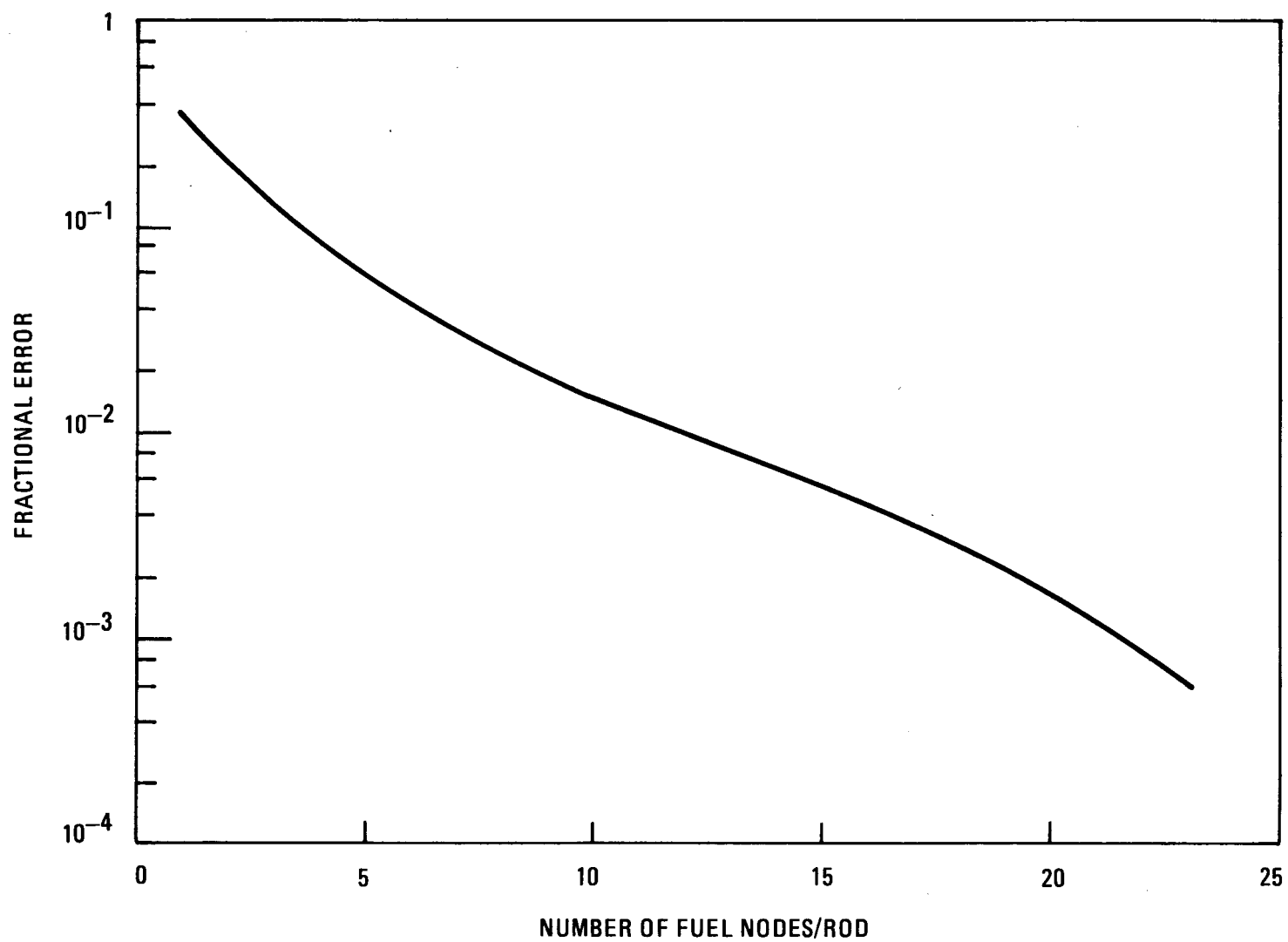


Fig. 13-3. Fuel temperature drop error at full power and steady state vs number of fuel nodes per rod

TABLE 13-6
VARIATION IN BOXRAD DUE TO NUMBER OF FUEL NODES

Node	T _{fuel} (°C)	T _{clad} (°C)	Time to Cladding Melt (s)	Time to Fuel Melt (s)
Case 1 ^(a)				
3	896 (9%) ^(b)	807 (7.5%)	108 (7%)	318 (3.3%)
5	848 (3%)	771 (2.6%)	113 (2.5%)	325 (1.2%)
15	821	751	116	329
Case 2 ^(c)				
3	844 (2.8%)	764 (1.7%)	114 (2%)	326 (1%)
5	830 (1.1%)	756 (0.6%)	115 (0.1%)	329
15	821	751	116	329

(a) Case 1 = steady state through 40-s coast-down followed by no flow to fuel melting (steady-state initial conditions calculated with same number of fuel nodes as used for transient).

(b) The percent denotes the change from the 15-node (exact) number.

(c) Case 2 = steady state through 40-s coast-down followed by no flow to fuel melting (all cases used 15-node steady-state calculations and then collapse to the number of nodes indicated for the transient).

13.4. POSTACCIDENT FUEL CONTAINMENT

During this quarter a core catcher feasibility study for the 1500-MW(e) GCFR plant was completed for the heavy metal bath and the borax bath concepts. Heat transfer analyses in the molten pool as well as lower shield and cavity liner were obtained. Previous analysis (Ref. 13-9) indicated that in a large GCFR plant, the requirements for an in-vessel core catcher are significantly more demanding than those for a demonstration plant. Therefore, the objective of this study was to establish a generic core retention concept which could be adopted to any plant size.

13.4.1. Plant Design Data for a 1500-MW(e) GCFR

Several commercial-size plants are currently under study at GA; only preliminary plant design data were available for the present study. The design parameters used for the large plant PAFC assessment are listed in Table 13-7. The increased difficulty in containing a molten core inside the PCRV of a large plant is primarily due to the fact that with increasing plant size, the core decay heat and fuel mass increased more significantly than the central cavity diameter. As a result, the pool layer thickness and the heat fluxes from the pool to the liner and into the PCRV cavity increase.

13.4.2. Assessment of a Borax Molten Core Containment System

The configuration of a proposed borax container for molten fuel and the lower reactor cavity for a 1500-MW(e) plant is shown in Fig. 13-4. Using the core catcher and borax container construction described in Ref. 13-10, the pool temperature can be maintained at 1427°C (melting point of stainless steel). At this temperature, molten borax can dissolve 40 vol % of the oxide fuel in 1 hr (Ref. 13-10). The time to reach complete melting of borax by adiabatic heating was estimated to be 6 hr; it was assumed that all the oxide fuel is dissolved in borax at the end of 6 hr. The molten oxide fuel increased the molten pool depth from 2.4 to

TABLE 13-7
PRELIMINARY DESIGN PARAMETERS FOR THE 1500-MW(e) PLANT USED IN THE
LARGE PLANT PAFC ASSESSMENT

Design parameters	
Reactor power	4168 MW(t) 1500 MW(e)
Core diameter	3.9 m
Core volume	17,368 liters
Outer diameter of radial blanket	5.7 m
Diameter of reactor cavity	9.4 m (estimated)
Fuel masses	
Core ($\text{UO}_2 + \text{PuO}_2$)	31,000 kg
Axial blanket (UO_2)	28,000 kg
Radial blanket (UO_2)	119,000 kg
Stainless steel volumes	
Core	2.27 m ³
Axial blanket	1.87 m ³
Radial blanket	2.55 m ³

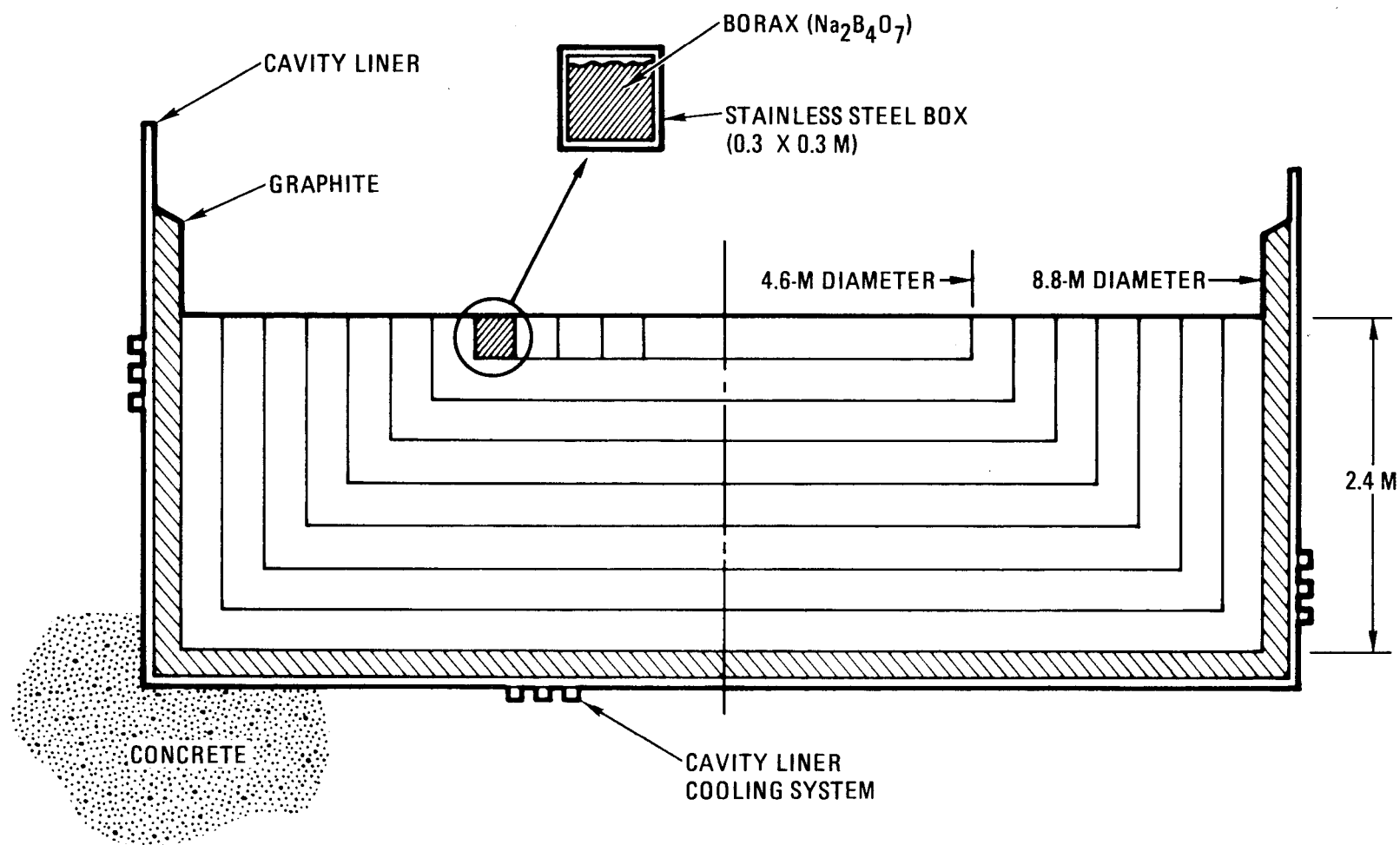


Fig. 13-4. Proposed borax core catcher for a 1500-MW(e) GCFR

2.7 m, and the stainless steel formed a solid layer 0.1 m thick at the bottom of the pool. The pool heat transfer was calculated using Baker's empirical model (Ref. 13-11), which includes the augmentation of upward heat flow by convective down-flow at the cooled sidewalls.

The decay heat generation in the melt at 6 hr is 32.2 MW (excluding heat sources from gaseous and volatile fission products). Of this heat, 23.3 MW were produced in the oxides and 8.9 MW in the stainless steel. The volumetric heat generation in the pool was $1.42 \times 10^5 \text{ W/m}^3$. The following molten borax properties were assumed for the compound solution:

Density	2040 kg/m^3
Specific heat	$2212 \text{ J/kg-}^\circ\text{C}$
Thermal conductivity	$7.4 \text{ W/m-}^\circ\text{C}$
Dynamic viscosity	0.4 kg/m-s
Coefficient of thermal expansion	$1.96 \times 10^{-4} \text{ }^\circ\text{C}^{-1}$

When these property values were assumed for the pool, the Kulacki-Goldstein Rayleigh number was 2.6×10^{11} . Heat transfer data for the molten pool for various cases were obtained (Table 13-8) using the iterative procedure illustrated in Baker's empirical model. The upper pool surface temperature was determined by radiation heat balance, and the sideward and lower pool surface temperature by cavity liner cooling conditions. Case A summarizes the results for the reference case at 6 hr, where gaseous and volatile fission products were assumed to have evaporated from the pool. All temperatures are quite low, and the sideward and downward heat fluxes (average) can be managed with engineered systems. However, local heat fluxes are higher and require further evaluation.

Some of the volatile fission products may be trapped by borax, forming a compound solution and thereby increasing the pool heat source. Case B assumed that at 6 hr the decay heat generation from the borax pool is increased by 30%. Only a slight increase in upper pool surface temperature is necessary to balance upward heat removal in the pool with radiative

TABLE 13-8
HEAT TRANSFER RESULTS OF BORAX CORE CATCHER STUDY

	Case ^(a)			
	A	B	C	D
Time after accident initiation (hr)	6	6	28	6
Heat generation from pool (MW)	23.3	30.2	20.5	30.2
Pool bulk temperature (°C)	1427	1427	1145	1427
Upper pool surface temperature (°C)	1282	1290	1052	1290
Sideward pool surface temperature (°C)	1139	1008	824	1008
Lower pool surface temperature (°C)	1120	1120	817	1120
Upward heat flow (MW)	9.3	9.2	3.5	10.1
Radiative heat removal capability (MW)	9.4	9.6	3.9	9.6
Sideward heat flow (MW)	13.0	19.9	15.5	17.6
Downward heat flow (MW)	9.9	10.0	7.4	10.2
Sideward heat flux (kW/m ²)	174	267	208	235
Downward heat flux (kW/m ²)	163	164	122	168
Sideward liner temperature (°C)	415	412 ^(b)	345	--
Lower liner temperature (°C)	397	399	324	--
Sideward graphite thickness (m)	0.10	0.06	0.06	--
Downward graphite thickness (m)	0.06	0.06	0.06	--
Percentage of heat flowing upward from pool	39.8	30.5	17.0	--

(a) Case A: with heat sources in gases and excluding volatiles; case B: decay heat generation from pool increased by 30% at 6 hr; case C: same as case B, but time is 28 hr after accident; case D: same as case B, but using Mayinger's correlation.

(b) With a factor two enhancement of the equivalent heat transfer coefficient from liner to cooling water.

heat transfer from the pool surface. The increased heat generation in the borax pool is mostly transferred sideward, resulting in a very high sideward heat flux and hence a very high cavity liner temperature. The cavity liner temperature can be maintained below the failure limit ($\sim 450^{\circ}\text{C}$) by enhancing the equivalent heat transfer coefficient (from liner to cooling water) by a factor of 2 [i.e., increasing the h value from $795 \text{ W/m}^2\text{-}^{\circ}\text{C}$ based on previously conservative calculations (Ref. 13-9) to $1590 \text{ W/m}^2\text{-}^{\circ}\text{C}$]. This improvement appears achievable.

Similar heat transfer calculations 28 hr after the accident are shown as case C in Table 13-8. Because of the decreasing decay heat rate, the pool temperature as well as the cavity liner temperature was significantly reduced. The fraction of upward heat removal was also greatly reduced, which indicates that the reactor internal structures may have sufficient heat capacity to absorb the remaining upward-flowing decay heat without helium convection. However, this needs to be demonstrated by a detailed upward heat removal analysis. As an additional comparison, case B was recalculated using Mayinger's correlation (Ref. 13-12) with the same pool bulk and surface temperatures; this is case D. The calculated heat balances for cases B and D agreed within 5%, but the heat flow distribution differed slightly. The upward heat flux in case D was greater than that in case B, but sideward heat flow was smaller. The differences were about 10%, which is within the $\pm 20\%$ accuracy range of Baker's empirical model.

13.4.3. Assessment of Heavy Metal Bath System for Molten Fuel Containment

The proposed heavy metal bath system is shown in Fig. 13-5. The core catcher diameter was assumed to be the same as that of the borax core catcher, i.e., 8.8 m. With a 2.0-m-thick uranium alloy base and a 0.2-m-thick preshield, melting was completed and a quasi-steady-state condition reached in 3.6 hr with a pool temperature of 1400°C .

The heat transfer mechanism in the uranium pool is complicated by the suspension of solid fuel pieces. Baker's empirical model was again used,

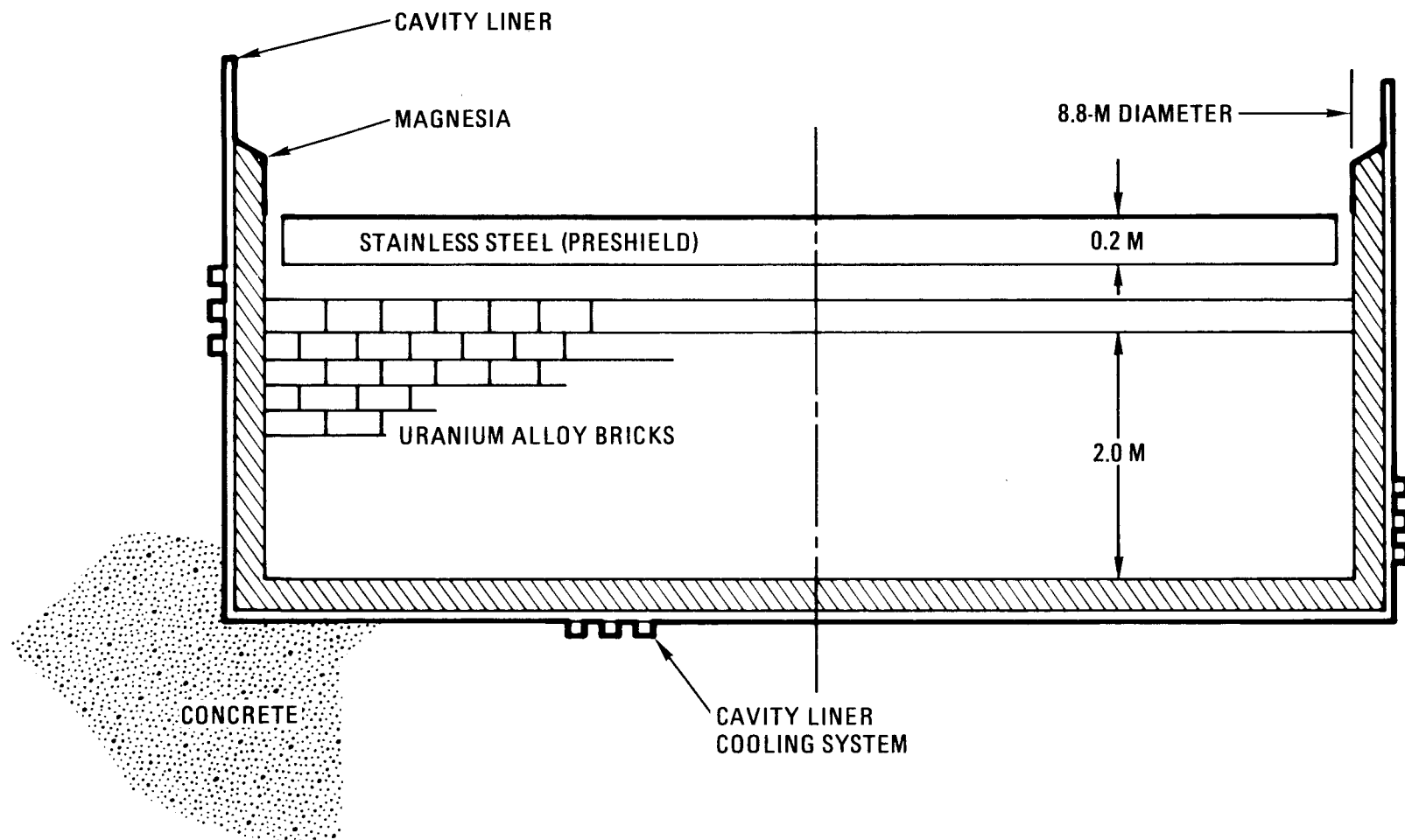


Fig. 13-5. Proposed heavy metal bath core catcher for a 1500-MW(e) GCFR

and the heat sources in the molten pool were assumed to be uniformly distributed. This assumption is conservative because it tends to underestimate the upward heat flow. At 3.6 hr after accident initiation, the decay heat rate was 42.5 MW; 30% of the decay heat sources were in the form of gaseous and volatile fission products, and 29.8 MW were generated in the melt. The volumetric heat generation rate was calculated to be $2.04 \times 10^5 \text{ W/m}^3$ with a pool thickness of 2.4 m. The following molten uranium properties were assumed for the heavy metal pool:

Density	$19,120 \text{ kg/m}^3$
Specific heat	$109 \text{ J/kg-}^\circ\text{C}$
Thermal conductivity	$35 \text{ W/m-}^\circ\text{C}$
Dynamic viscosity	0.004 kg/m-s
Coefficient of thermal expansion	$6 \times 10^{-5} \text{ }^\circ\text{C}^{-1}$

The Kulacki-Goldstein Rayleigh number with these property values was 1.21×10^{12} . Heat transfer results for the heavy metal pool at 3.6 hr were obtained as follows using the iterative procedure illustrated in Baker's empirical model:

Pool bulk temperature	1400°C
Pool upper surface temperature	1375°C
Pool side surface temperature	1346°C
Pool lower surface temperature	1100°C
Remaining solid uranium alloy	0.2 m
Upward heat flow	11.8 MW
Sideward heat flow	15.3 MW
Downward heat flow	2.7 MW
Sideward heat flux	213 kW/m^2
Downward heat flux	44 kW/m^2
Cavity liner temperature	349°C (sides) 188°C (bottom)
Magnesia thickness	0.04 m (sides) 0.12 m (bottom)
Heat flowing upward from pool	40%

Because of the high sideward heat flux, the cavity liner temperature at the sides was calculated based on an improved heat transfer coefficient, as in case B of the borax core catcher study. The results calculated above appear to support the feasibility of the heavy metal bath concept.

The heavy metal bath system inherently eliminates the possibility of structural components floating to the pool surface. The density difference can be further exploited to provide a self-sealing feature which reduces (if not eliminates) the concern for molten fuel penetrating into narrow cracks of the core catcher structure. Compared with the borax bath concept, the melting process used in forming a heavy metal bath is more dependable than the dissolving process used in forming a compound solution pool. In addition, the heavy metal pool temperature can be managed in a wider range, whereas in a borax bath the pool temperature is limited by the dissolving rate of the oxide fuel in borax and the evaporation rate of borax. However, the shorter melting time due to the low heat capacity of uranium alloys and the nonuniform heat distribution over the lower and sideward cavity liner are considered to be a disadvantage of the heavy metal bath concept.

13.5. ENGINEERING RELIABILITY INTEGRATION

Existing and past reliability programs and analysis methods are reviewed in Ref. 13-13, and the role of reliability engineering is discussed. A sample problem will be analyzed during the next quarter using the principles identified in Ref. 13-13 and shown in Fig. 13-6. The reliability methods to be employed include

1. Failure modes and effects analysis (FMEA).
2. Fault tree analysis (FTA).
3. Reliability diagrams.
4. Logical reduction of fault trees (Boolean algebra).
5. Constant failure rate reliability calculations (LAMBDA-TAU).

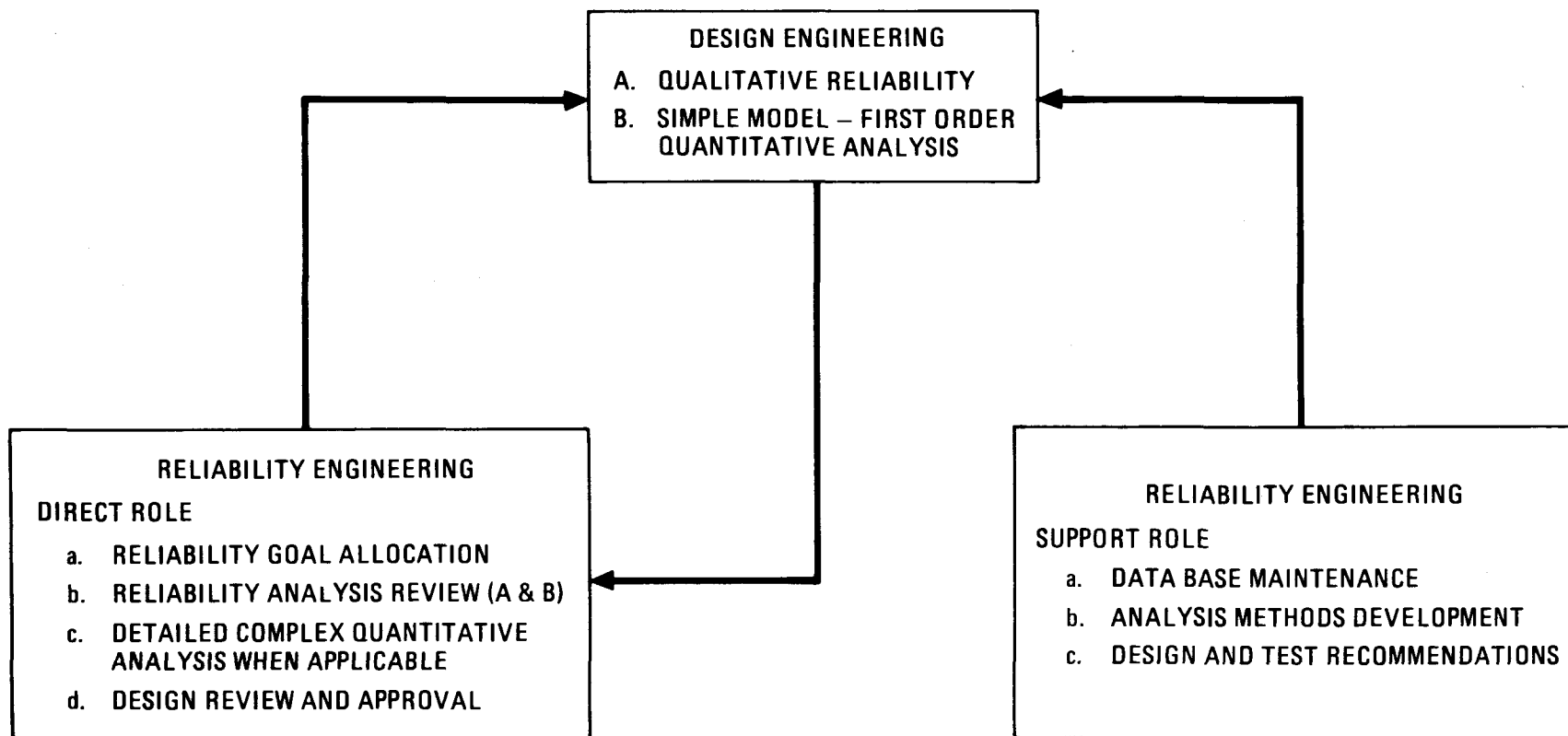


Fig. 13-6. Role of reliability engineering in the design cycle

13.6. GAS-COOLED REACTOR RELIABILITY DATA BANK

The functions of the data bank are to obtain, supply, and store reliability data in support of the probabilistic accident analysis performed under the GCFR and HTGR probabilistic accident and risk analysis tasks.

13.6.1. Data Sources

During the last quarter two new sources of reliability data were obtained by the data bank. Reference 13-14 contains failure rate estimates derived from expert opinion refined by the Delphi process. The data are organized so that high-level generic component failure rates can be used when failure rates for more specific components have not been estimated. In addition, low, recommended, high, and maximum ranges are included along with environmental stress factors. These stress factors are multiplied by the failure rate when the components are used under particular environmental conditions.

Reference 13-15 provides data on common cause failures in the redundant emergency core cooling system and reactor protection systems of pressurized water reactors (PWRs) and boiling water reactors (BWRs). The data in Ref. 13-15 are a valuable contribution to the common cause failure data stored in the data bank. Standard sources (Refs. 13-16 through 13-18) maintained by the data bank and data from European gas-cooled reactors have been updated.

13.6.2. Data Analysis

13.6.2.1. Failure Mode Frequency Ratio Estimates for Fail to Start and Fail to Load for CACS Components. The ratio of the failure rate for failure mode i (λ_i) to the total failure rate (λ_T) is required to assess the safety reliability advantages of alternate standby operating modes for the CACS (see Section 13.2.2). The basic data for the companies were taken

from Ref. 13-19. The basic objectives of this study were to analyze documented occurrences to develop the running failure rate of an idling component as a fraction of the full-load running failure rate and to develop the failure rate of an idling component for picking up load on demand as a fraction of the total demand failure rate for starting and loading. Table 13-9 summarizes the results of this analysis.

The low-load running failure rate of a constant-speed electric drive/pump was 0.96 of the running failure rate of a pump and drive. Its fail-to-load failure mode frequency ratio (FMFR) was ~ 0.025 , or $\sim 1/40$ of the demand failure rate of a pump and drive. The idling running failure rate of a variable-speed electric drive/pump was 0.75 of the running failure rate of a pump and drive. Its fail-to-load FMFR was ~ 0.33 , or $\sim 1/3$ of the demand failure rate of a pump and drive.

No data are available for a CACS auxiliary circulator. However, excluding the control, the variable-speed drive and circulator are probably less complex than a variable electric drive and pump but more complex than a variable electric drive and fan. With an idling variable electric drive and pump fail-to-load of $\sim 1/3$ of the demand failure rate and an idling variable-speed electric drive and fan fail-to-load of $\sim 1/6$ of the demand failure rate, an estimate of $1/5$ the demand failure rate seems to be reasonable for the CACS auxiliary circulator motor and circulator.

13.6.2.2. Abrupt Loss of Circulators. During the time required for the CACS to start on demand, reliance is placed on the main loop cooling system to perform the RHR function. The basis of start-up for the CACS is a common cause main circulator coast-down due to mechanical inertia alone. Therefore, to meet the RHR reliability target, the probability of a common mode loss of all main circulators should be on the order of $10^{-6}/\text{yr}$ or less. A preliminary analysis of airplane engine reliability data (Ref. 13-15) and very severe turbine generator failures (Ref. 13-20) indicates that the probability of a rapid loss of rotation is $\sim 2.4 \times 10^{-4}/\text{yr}$, and a common mode failure fraction of 0.011 appears to be achievable

TABLE 13-9
FAILURE MODE FREQUENCY RATIO ESTIMATES

	Start and Load Problems	Load Problems/ FMFR (a)	Low-Load or Idling Problems/ FMFR
Generic drive/pump	93	38/ ~ 0.4	12/ ~ 0.13
Steam turbine drive/ pump	54	14/ ~ 0.26	11/ ~ 0.2
Constant-speed electric drive/pump, low-load	39	24/ ~ 0.6	1/ ~ 0.026
Variable-speed electric drive/pump, low-load	39	24/ ~ 0.6	13/ ~ 0.33
Diesel generator (per demand)	0.01 + 0.015	0.015/ ~ 0.6	--
Electric drive/fan (constant- and variable-speed)	6	1/ ~ 0.17	1/ ~ 0.17
Damper	19	3/ ~ 0.16	2/ ~ 0.1

(a) FMFR = failure mode frequency ratio.

for well designed equipment with an extensive operating record. Thus, the probability of a common mode rapid failure of all circulators could approach 3×10^{-6} /yr. Since this failure probability is slightly higher than the target rate, a more detailed data analysis has been initiated, and CACS options which place less reliance on the main loop bridging function are being investigated.

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14. GCFR SAFETY TEST PROGRAM (189a No. 00588)

It is the responsibility of GA to coordinate the National GCFR Safety Test Program; GA reviews and directs the program so that it is responsive to safety test needs and identifies new test needs for which test plans must be proposed and implemented on a time scale consistent with GCFR program needs.

14.1. GRIST-2 PROGRAM

The GCFR Safety Program Review Committee has recommended that GCFR fuel tests be undertaken in a transient facility to investigate fuel behavior during unprotected loss of flow and reactivity insertion transients. Therefore, the GRIST program is being developed to complement analytical and experimental programs being conducted under other GCFR and LMFBR programs.

During this quarter, the GRIST-2 program management plan was drafted, and the organizational responsibility structure for the program was defined. Several organizational responsibility structures were considered; of these, two options were most attractive. [The tasks described below are based on the proposed work breakdown structure developed for the GRIST program management plan (Fig. 14-1).] The first option is essentially the current organization (see case 1, Table 14-1) with EG&G having responsibility for the helium supply loop and in-pile tube design and construction and primary responsibility for supporting tasks in physics, safety, and facility integration. The responsibility for test train design and test planning remains with ANL, which is also responsible for physics, safety, and facility integration. The second option (case 2, Table 14-1) shifts the responsibility for in-pile tube design and construction to ANL to consolidate the design of the entire

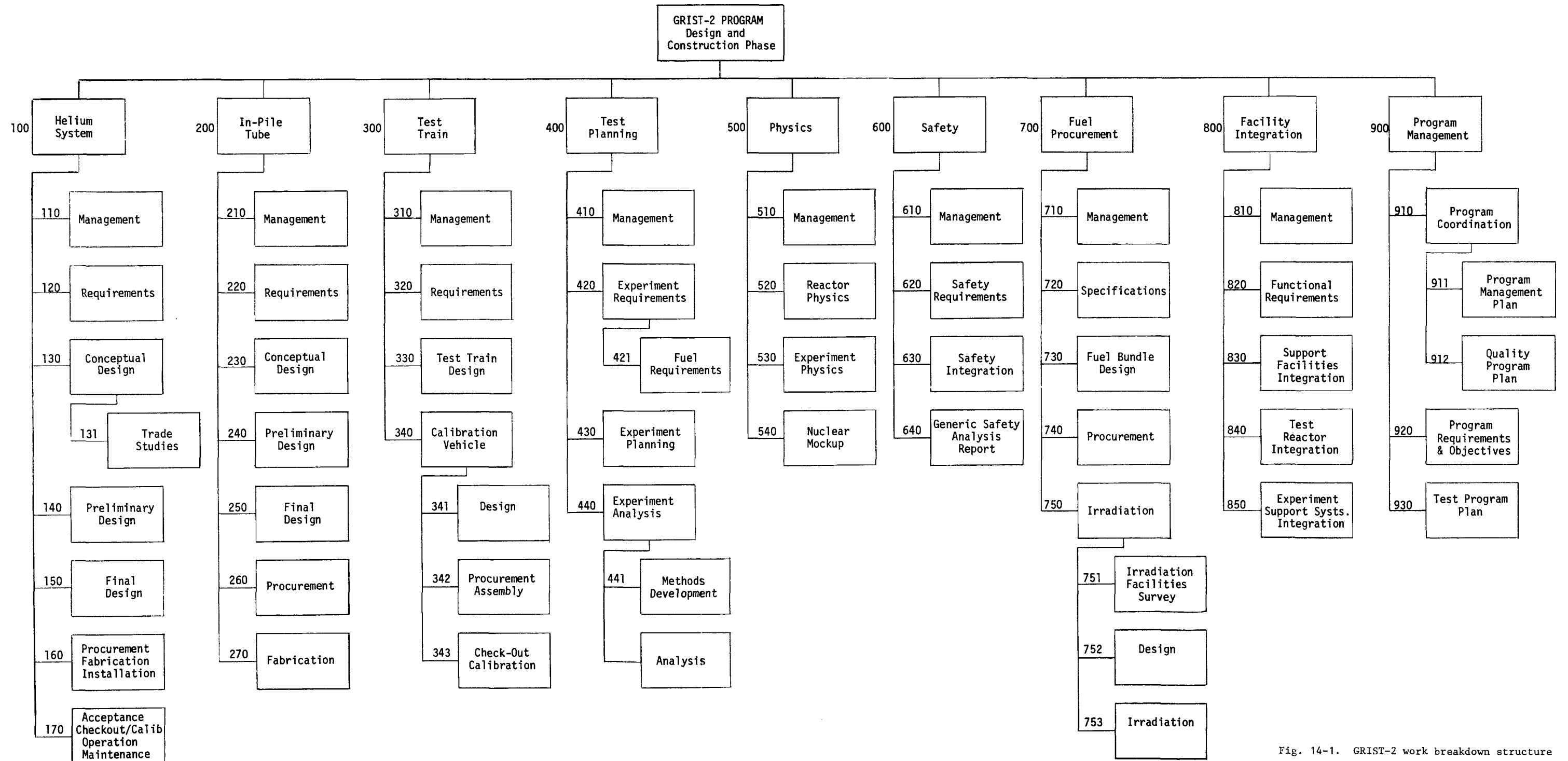


Fig. 14-1. GRIST-2 work breakdown structure

TABLE 14-1
ORGANIZATIONAL RESPONSIBILITY OPTIONS EVALUATED FOR THE GRIST
PROGRAM DESIGN AND CONSTRUCTION PHASE

Task	Responsibility ^(a)		
	Case 1	Case 2	Case 2a ^(b)
100: helium system	EG&G	EG&G	ANL-E/(EG&G)
200: in-pile tube	EG&G	ANL-E	ANL-E
300: test train	ANL-E	ANL-E	ANL-E
400: test planning	ANL-E	ANL-E	ANL-E
500: physics	EG&G/(ANL-E)	ANL-E	ANL-E
600: safety	EG&G/(ANL-E)	ANL-E/(EG&G)	ANL-E/(EG&G)
700: fuel procurement	GA	GA	GA
800: facility integration	EG&G/(ANL-E)	ANL-E/(EG&G)	ANL-E/(EG&G)
900: program management	GA	GA	GA

(a) Organization in parentheses has responsibility for ongoing work on task but does not have prime responsibility for task.

(b) Organizational responsibility structure recommended by ANL-E.

in-pile section under one organization. With this change, it logically follows that ANL should be responsible for all physics work, since physics support is not needed by the helium supply loop task. The primary responsibility for the safety task and possibly the facility integration task would be transferred to ANL, since a large portion of these task efforts goes into supporting the in-pile tube and test train tasks. The responsibility for the helium supply loop system would remain with EG&G along with supporting work in safety and facility integration, with the latter tasks being coordinated by ANL. This second organizational responsibility structure closely parallels that recommended by ANL (Ref. 14-1), with the exception that in the ANL recommendation the responsibility for the helium supply loop system is under ANL's jurisdiction and the work is subcontracted to EG&G (case 2a, Table 14-1).

Two different approaches were used to evaluate the organizational structures to select the option which would best serve the needs of the GRIST program. First, an attempt was made to quantify the costs which would result from the interorganization interfaces and duplication of work within the supporting tasks. The cost difference between these two options was found to be about 2% of the total for the design and construction phase, with the second option (case 2) having the lower cost. A difference of 2% amounts to approximately \$500,000, which does not appear to give a clear advantage to either organizational responsibility matrix.

The second approach involved an objective analysis of the complexities of the different cases to determine the amount and type of work required of each organization. Special attention was given to the physics and safety task since these tasks appear to be the fulcrum point on which the recommendation is balanced. The main conclusion of this analysis was that ensuring a smooth and direct exchange of technical information is the principal concern of both options. If effective and direct technical communication can be established, then the current organization can provide a continuous and ongoing system of checks and balances to assure early identification of problem areas during the design and construction phase of the program.

In addition to the above analyses, other factors also influenced the GA recommendation. First, there are definite incentives to maintaining a strong role at EG&G and ANL during the design and construction phase of the GRIST-2 program. EG&G has demonstrated abilities and experience in gas loop system design and construction project management, and ANL has a unique background because of its role as the primary experimenter for the LMFBR safety testing programs. Second, the current organization will be able to supply the hardware necessary to carry out a meaningful experimental program and still meet the desired initiation of testing date of mid-1983. Shifting the major task efforts to ANL (case 2) at this time would cause an estimated delay in schedule of between one and two years. In addition, the current organization (case 1) allows adequate involvement by ANL to ensure, through continued review, that the experimenter's requirements are met.

Based upon the above considerations, it is recommended that the current organization (case 1) with some modifications be adopted if the conditions listed below can be implemented.

1. The primary responsibility for the design and construction of the helium supply loop system and in-pile tube should remain with EG&G.
2. ANL, in the role of experimenter, should retain the responsibility for test planning and test train design, including design and construction of the calibration vehicles for thermal-hydraulic and nuclear checkout and calibration of the test train design. The primary responsibility for the physics task should be transferred to ANL.
3. As a means of implementing effective technical communication among EG&G, ANL, and GA, a technical coordinating committee should be formed. This committee should consist of two representatives from EG&G, ANL, and GA, and the chairmanship of this committee

would be part of GA's responsibility for overall technical program management. The committee would set up procedures to expedite and control direct technical information exchange among the principal program participants. Technical problems would be referred to the committee for resolution, and in the event of an impasse, the matter would be referred to DOE for arbitration. Recommendations for contractual changes should be developed, and the management committee should approve them.

4. As its first order of business, the technical coordinating committee should submit a recommendation to DOE which defines the responsible organizations and work scopes within each organization for the physics, safety, and facilities integration tasks, since both EG&G and ANL will need internal support in these areas.

If it is not feasible to establish the technical coordinating committee or an equivalent technical management framework among GA, EG&G, and ANL, the second option (case 2), where the prime responsibility for in-pile tube design, physics, and facility integration is transferred to ANL while maintaining EG&G at the full program participant level during the design and construction phase, should be adopted. In this case, a schedule delay and some work repetition appears unavoidable.

14.2. DUCT MELTING AND FALLAWAY TEST PROGRAM

14.2.1. Program Status

During this quarter the design of the 271-rod bundle test fixture and procedures for conducting the first full-length subgroup test (FLS-1) of a 37-rod bundle were reviewed. The purpose of the 271-rod bundle fixture review was to freeze the design of a number of long lead-time procurement

items so that design and procurement of these items can begin on a schedule consistent with that for the first 271-rod test. In addition, the design was reviewed to ensure that all the requirements for the duct melting and fallaway test (DMFT) and depressurization accident condition tests could be fulfilled. Los Alamos Scientific Laboratory agreed that the following items should be considered in the design of the test fixtures:

1. Allowance for fuel rod pitch variations between 9.8 and 11.2 mm. LASL will use a value of 10.18 mm in their design of the first DMFT test.
2. Allowance for interduct wall spacings of 6 to 26 mm.
3. Provision of x-ray viewing ports at specified locations for bundle examination before, during, and after a DMFT or depressurization accident condition test.
4. Provision of a sufficient gas channel inlet and exit length to establish flow stability before helium enters the core region.
5. Consideration of the need for easy disassembly and reassembly of the test fixture to facilitate depressurization accident condition bundle inspection and requalification between runs.

The possibility of having GA supply prototypical bundle components, including spacer grids, ducts, cladding, and corner support rods, for the DMFT and depressurization accident condition tests was proposed. Los Alamos Scientific Laboratory will evaluate this proposal.

The procedures for the first 37-rod test (FLS-1) were reviewed. This will be the first full-length subgroup test. Attention was given to the test operation analysis performed by GA which predicted temperature distributions within the bundle for the various tests that are planned. A series of natural convection tests will be conducted prior to simulating a PLOF

accident to test the heater rod capabilities. The results from the natural convection tests will be used to predict convective cooling effects during the full-size bundle test; this is a necessary step in evaluating heater rod performance in the larger tests. The analytical predictions for this first test are presented in Section 14.2.2.

14.2.2. DMFT Analysis

Development of the BOXRAD computer code (Ref. 14-2) for PLOF studies (Section 12.3.4) was followed by BOXRAD code extensions to model the specifics of the first 37-rod DMFT experiment (FLS-1). The extended code was then applied to the proposed LASL test procedures. The most important result of the current study is that it confirms the earlier prediction (Ref. 14-3) that the alumina insulators in some of the electrically heated rods could melt during the test.

The five general areas of code development involved (1) a radiative heat transfer model that could evaluate alumina translucence; (2) a natural convection algorithm that could conservatively establish that the alumina would reach its solidus; (3) an insulation model; (4) addition of three new material types (i.e., a surface node for radiative heat transfer, alumina, and variable-diameter graphite for the heater rod); and (5) a graphics routine. The radiative heat transfer model developed for the DMFT analysis allows (radial) energy transfer only between radially adjacent materials. (The "circumferential" gap between rods would allow any three radially adjacent materials to interact, but the interaction with the adjacent rows would be at least three times as large, and the calculational complexity of the general case would be particularly difficult with translucent alumina in the heater rods.) The six possible radiative modes are as follows:

1. Within a clad heater rod.
2. Between clad heater rods.
3. Between a clad heater rod and a duct wall.
4. Between a declad heater rod and a clad rod.

5. Between a declad heater rod and a duct wall.
6. Between two declad heater rods.

The PLOF (fueled rod) version of BOXRAD considers only modes 2, 3, and a simpler version of 4 through 6 (i.e., no translucence).

Analyses were completed which determined the effectiveness of natural convection within the bundle to delay the calculated onset of alumina melting. The assumptions used in the BOXRAD natural convection model are not always physically valid; rather, they limit the potential for heat removal from the bundle. The BOXRAD natural convection model evenly divides the duct area into a central up-flow channel and an outer down-flow annulus along the duct wall. The entire flow is postulated to travel from a common point above the upper axial blanket, down along the duct wall without radial gas mixing, all the way to the cladding blockage. At the blockage, the down-flow channels are mixed to a common temperature for up-flow through the assembly center. The entire up-flow raises the total length of the upper axial blanket before the channels are mixed for input to the down-flow at the next integration step. Since all locations away from the axial centerline (both up and down) are colder than the centerline, the assumption of full-length flow violates the physical process laws which have the axial flow varying according to the radially averaged axial temperature. Thus, the magnitude of the time-varying and axially varying flow is exaggerated, and the available thermal inertia is the theoretical maximum (e.g., for the entire assembly above the upper axial blanket).

The following information has been developed using the heater rod version of BOXRAD for implementation in the LASL test procedures:

1. A recommended input power transient for heating and holding the centerline cladding temperature at 800°C (Fig. 14-2).
2. A recommended input power transient for preheating the axial and radial centerline cladding temperatures to 800°C at 4.6%

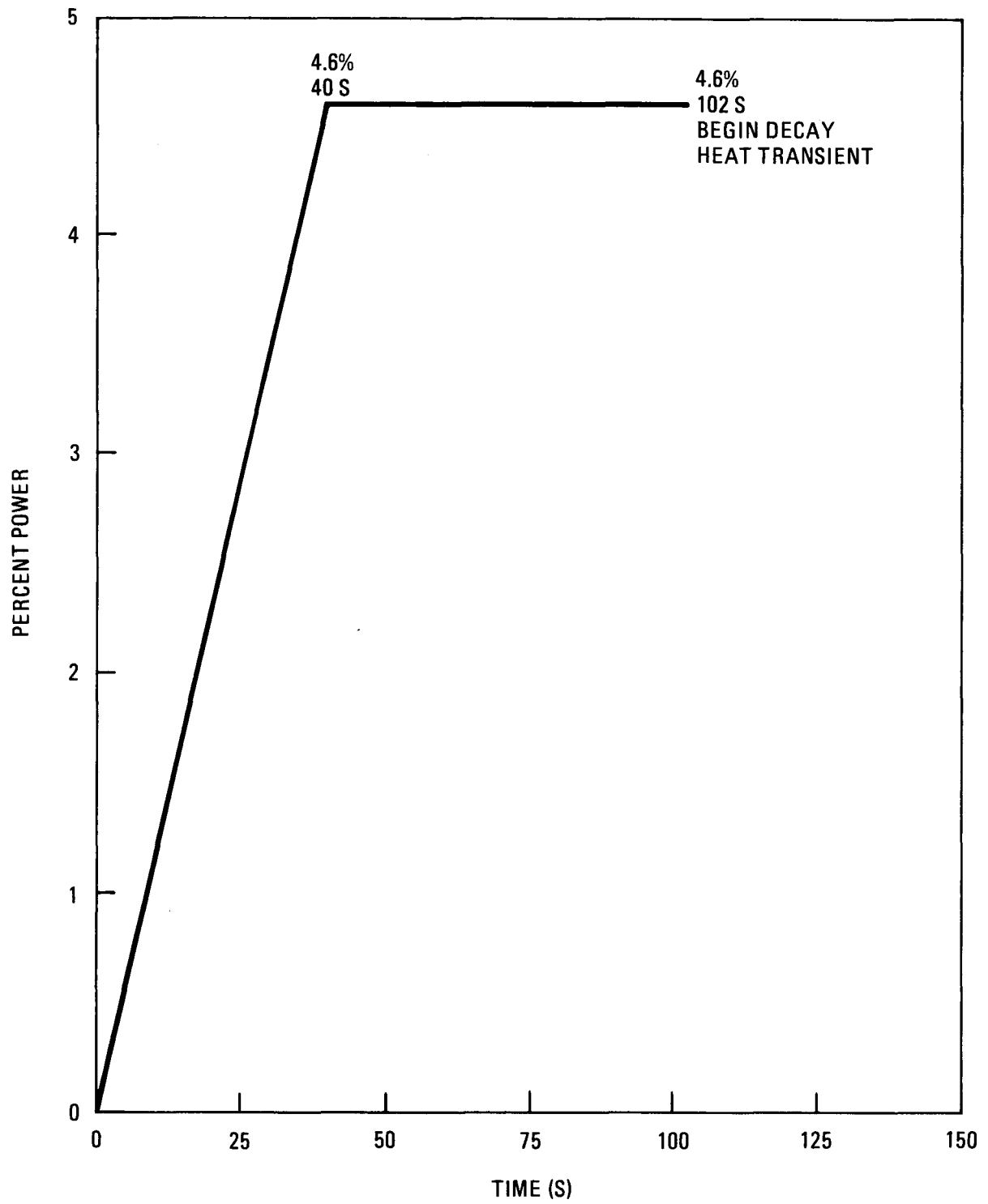


Fig. 14-2. Recommended preheat transient for testing to destruction

power for immediate continuation with the time-varying decay heat starting 40 s after scram. The preheat transient is shown in Fig. 14-3.

3. Data on step responses, cooldown times, and preheat procedures to 400°C.

The use of this new, more sophisticated model confirmed earlier predicted alumina overtemperatures within the time interval preceding duct melting (Ref. 14-2). Figure 14-4 shows that alumina melting is imminent within 200 s after the beginning of the decay heat transient. Figure 14-5 shows the time progression of the radial cladding temperature with a continuous 0.1% natural convection loop flow, beginning when cladding blockage could form in the lower axial blanket. The tenth of a percent flow corresponds to an axial velocity of 22 m/s; this value is expected to be well outside any reasonable natural convection loop flow. The DMFT time interval of interest is 300+ s after test initiation, or 400+ s from the beginning of the preheat transient. Thus, prediction of melting of the Al_2O_3 sleeves with this conservative natural convection flow implies that a significant amount of alumina could melt under the conditions of this first DMFT run.

REFERENCES

- 14-1. Avery, R., "GRIST-2 Interfaces and Organizational Responsibilities," Argonne National Laboratory, private communication.
- 14-2. Emrich, W. J., Jr., "Duct Melting in the GCFR," General Atomic Company, unpublished data.
- 14-3. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period November 1, 1977 Through January 31, 1978," DOE Report GA-A14771, General Atomic Company, February 1978.

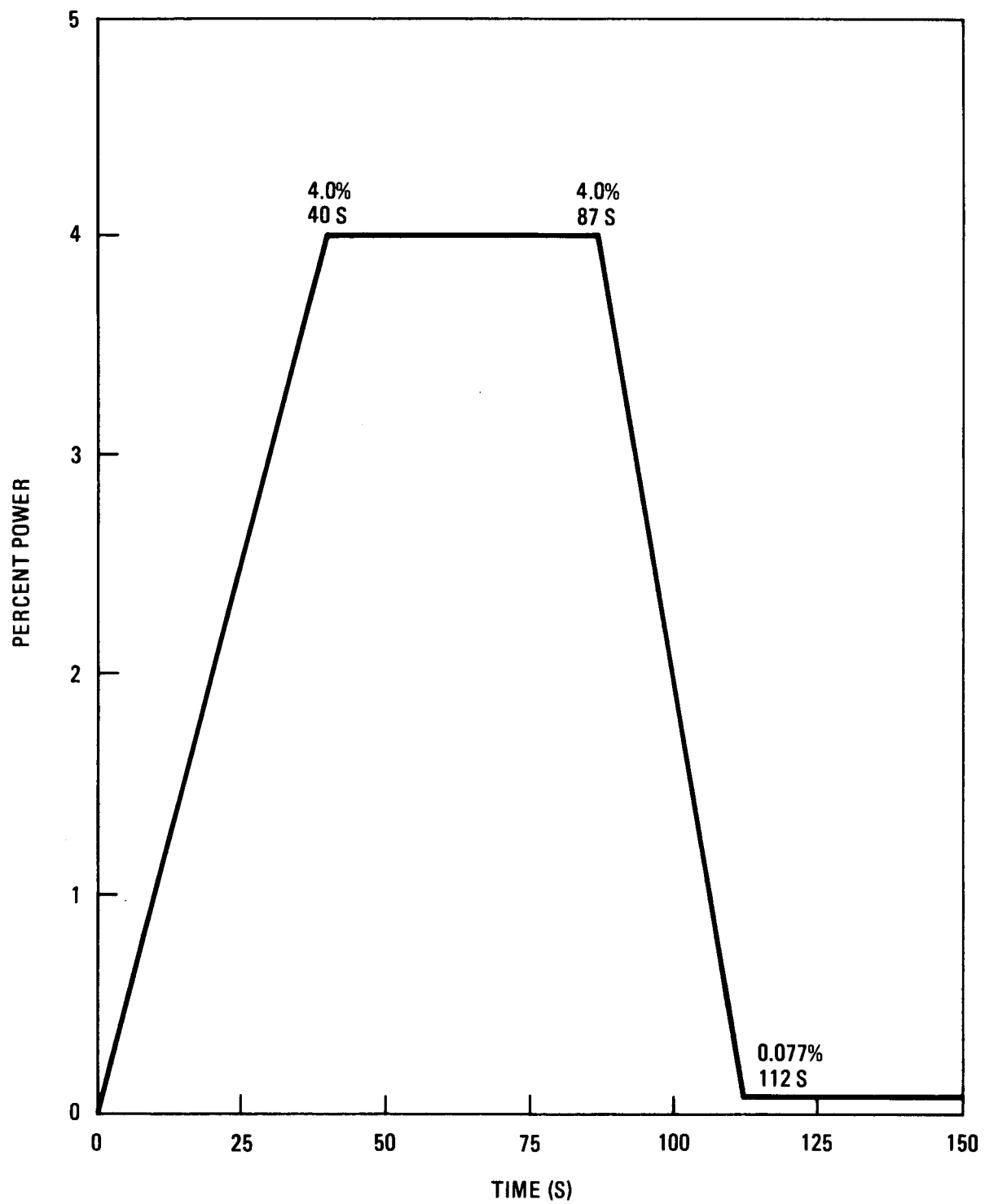


Fig. 14-3. Recommended power transient for holding at 800°C

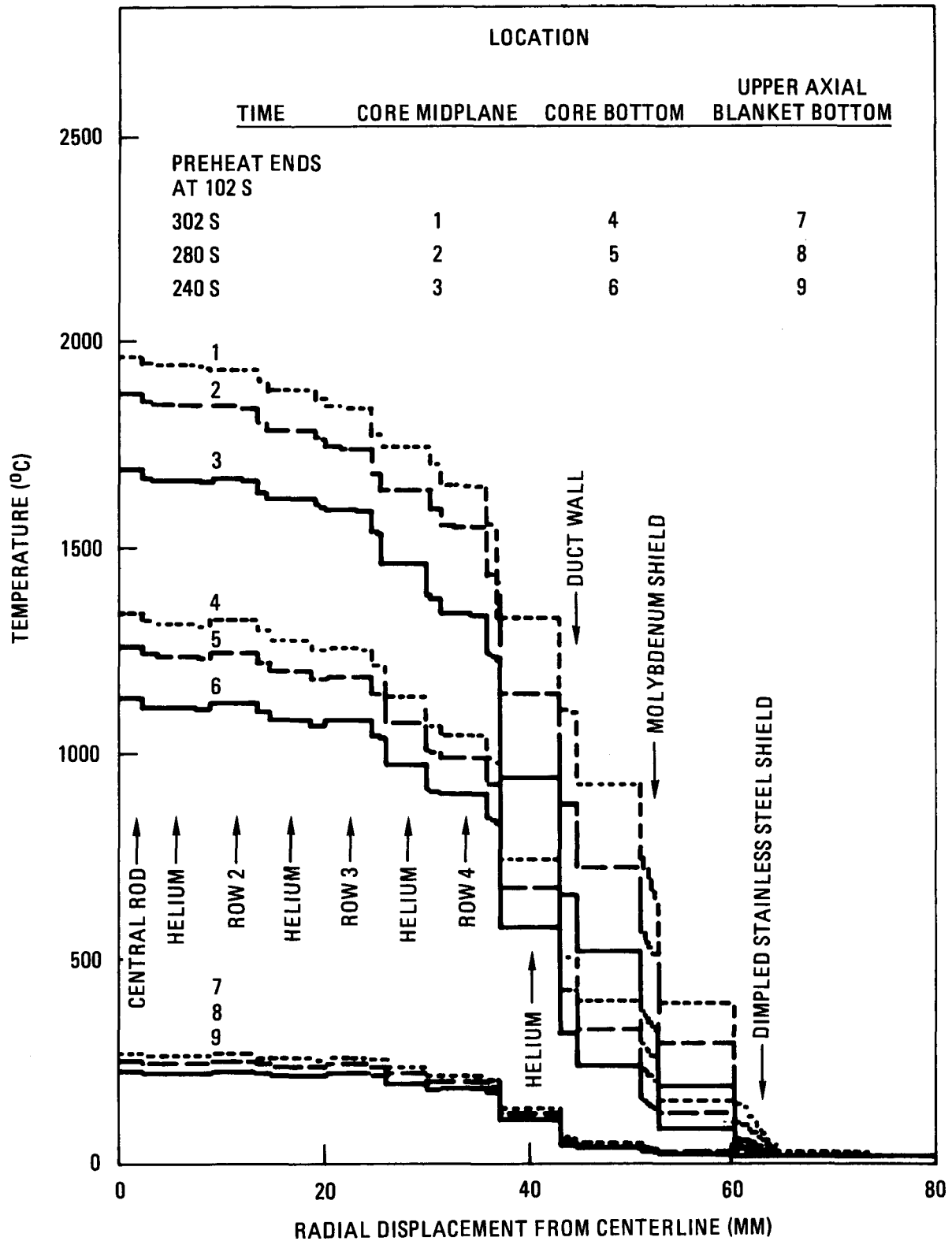


Fig. 14-4. Radial temperature profile from centerline through pressure vessel as a function of time and axial location; natural convection is not calculated, and radial locations greater than ~43 mm are outside the duct wall. [Compare (especially) the centerline bottom upper axial blanket temperatures with the temperatures in Fig. 14-5.]

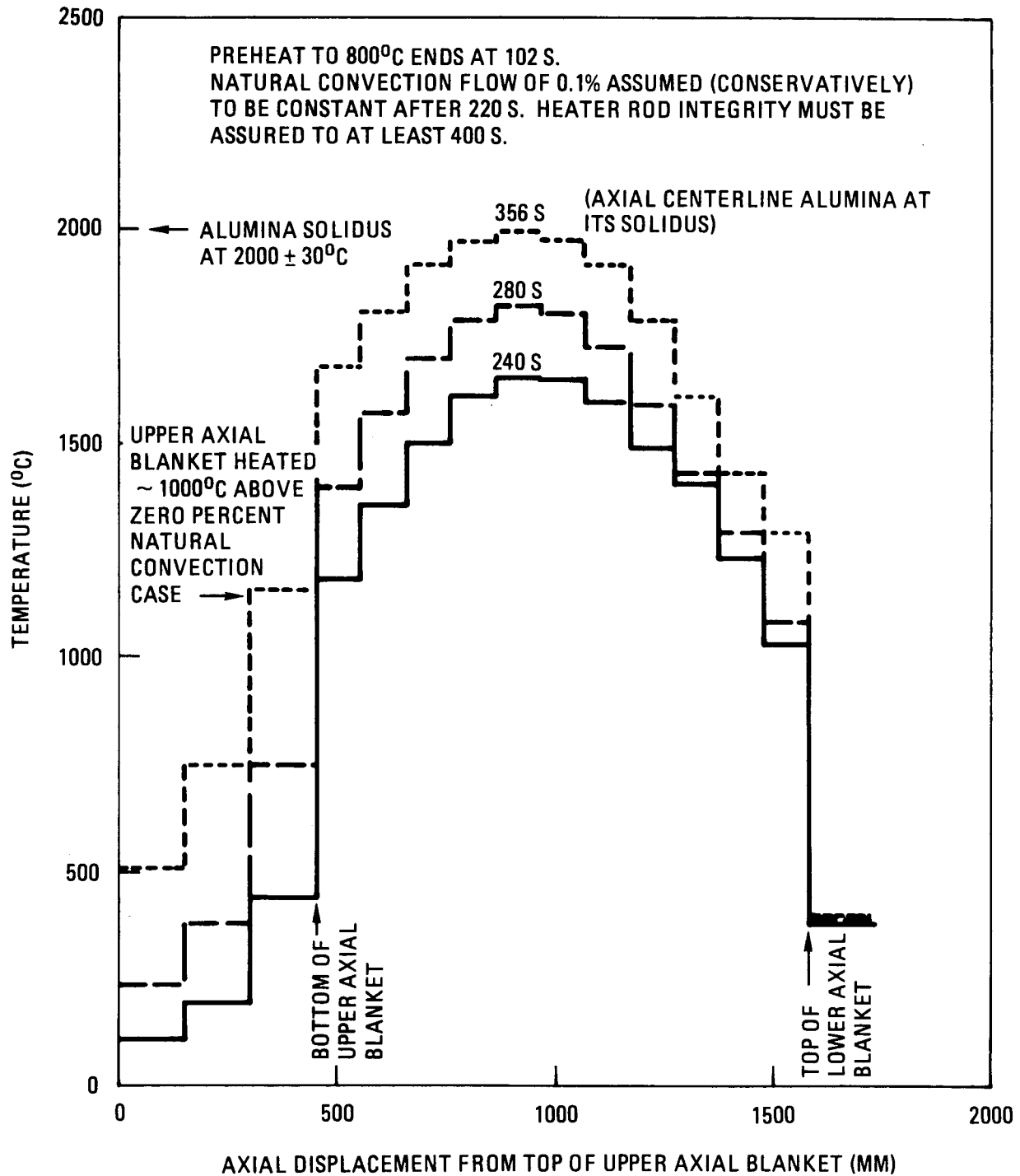


Fig. 14-5. Central rod axial temperature profile as a function of time. [All curves are for cladding on the central rod; after the cladding melts (at 425°C), the replacement helium gas temperature is plotted.]

15. GCFR NUCLEAR ISLAND DESIGN (189a No. 00615)

The purpose of this subtask is to provide the general arrangement of the nuclear island so that the feasibility of nuclear island concepts and the major dimensions of the buildings can be established.

An electrical specification (Ref. 15-1) was prepared for the proposed GCFR circulator test facility. This document incorporates the standard GA criteria employed in HTGR development, with appropriate modifications to suit a GA-owned facility.

In accordance with containment layout practice established for the HTGR development program, it was decided to decrease the diameter of the reactor containment building from 36.48 m (120 ft) to 35.26 m (116 ft). This size provides a 4.86 m (16 ft) annulus space between the 25.54-m (84-ft) diameter PCRV and the containment wall. The smaller containment decreases the net free volume, which in turn increases the back pressure following a DBDA. The higher back pressure is compatible with CACS requirements.

Work was started on the layout of the reactor containment building (RCB), and a criterion was established for orienting the reactor core relative to the PCRV and its associated NSSS components. This criterion is essential for plant layout work (Ref. 15-2). The identification system will be incorporated into future design documents.

The BOP section of the Electric Power Research Institute (EPRI) 300-MW(e) GCFR plant assessment (Ref. 15-3) was reviewed. The EPRI assessment was based on the BOP design developed by Bechtel in 1973 (Ref. 15-4); hence, many of the original features are now out of date. Valid EPRI suggestions will be incorporated in the revised BOP design.

A cost comparison for a circular and square confinement building was performed. Estimates show that a square configuration is approximately six million dollars cheaper than a cylindrical structure. Although this gives an advantage to a square confinement building, final selection is dependent upon functional requirements, i.e., pressure retention capabilities. The probability of a high-energy pipe rupture within the confinement and subsequent pressure build-up will be analyzed before a decision is reached.

The feasibility of locating the CACS heat exchangers on top of the control and diesel generator building was studied. It was determined that this arrangement did not impair system safety requirements, nor was the seismic criterion jeopardized. It was decided to proceed with this location for the CACS air blast heat exchangers.

A preliminary layout of the 1200-MW(e) plant containment building was prepared. The main purpose of the layout is to demonstrate this feasibility of routing major piping without infringing on the refueling area under the PCRV. The layout proves that adequate space is available to achieve the required routing of pipes and accomplish necessary access for maintenance and in-service inspection.

The RECS logic diagram and schedule data for the nuclear island preliminary design were completed.

REFERENCES

- 15-1. "GAC Statement of Work, Conceptual Design of the GCFR Main Helium Circulator Test Facility, Rev. 1," General Atomic Company, unpublished data.
- 15-2. Flanagan, V. D., "GCFR PCRV Component Orientation," General Atomic Company, unpublished data.
- 15-3. "EPRI Review of GCFR Development Plans," Electric Power Research Institute, unpublished data.
- 15-4. "300 MW(e) Gas Cooled Fast Breeder Reactor Balance of Plant Preliminary Engineering and Cost Estimate," Bechtel Job 10437, August 1973.

16. ALTERNATE DESIGN STUDY (189a No. 00759)

The objective of this task is to develop and investigate alternate design concepts for the GCFR 300-MW(e) plant NSSS configuration, components, and related plant facilities and equipment. The major conclusions and recommendations of the study are presented in Ref. 16-1 and discussed below.

16.1. NONINTEGRATED NSSS DESIGN

The basic nonintegrated NSSS arrangement studied utilizes a PCRV, with the steam generators, main circulators, and auxiliary core cooling system located in steel vessels outside the PCRV. Concentric ducts are used to transfer the helium coolant between the PCRV and the steam generator and CACS vessels. The hot gas from the core is carried within the inner duct, whereas the cold, high-pressure return gas flow passes through the outer duct. Because of the large cross section of the ducts, double-ended pipe failure of one or both ducts could produce unacceptably rapid depressurization and possibly lead to disruption of the reactor internals. Therefore, it is necessary to provide an external flow limiter outside the ducts in the form of an outer duct with sliding joints for thermal movement between the components. Burst protection for failure of the code-designed steel pressure vessels containing the steam generator and the CACS was not considered.

It was concluded that the basic nonintegrated configuration does not provide an improvement in reliability and integrity compared with the multicavity PCRV design presently proposed for the GCFR reference design. Particular concerns are associated with the design of the concentric ducts and the incompatibility of the duct design requirements at the PCRV with those at the surrounding steel vessels. Critical safety requirements must be satisfied for failure of the steel cross ducts as the design basis event.

The high thrust loads resulting from a cross duct failure must be accommodated by the vessel support structure, and damage to the containment from internally generated missiles, which could lead to leakage of active coolant, must be prevented. Overall, the nonintegrated design with separation of the external steel vessels from the PCRV would lead to severe disadvantages in terms of safety. It is not clear whether these could be overcome without incurring economic penalties.

Another significant conclusion is that for the nonintegrated design with a PCRV, the commercial plant would be restricted to a three- or four-loop design. This results from the space limitations required in the PCRV to accommodate the concentric ducts for the main and auxiliary loops.

16.2. HOT LINER STUDY

In principle, the hot liner for the PCRV consists of a thin steel liner backed by a layer of insulating concrete surrounded by the normal structural concrete. No thermal barrier or cooling system is required to be attached to the hot liner. The absence of the thermal barrier on the gas side of the liner offers the possibility of more direct access to the liner for inspection and possible repair. A cooling system is required at the interface between the insulating and the structural concrete. The hot liner concept was analyzed, and the advantages of bare liner inspectability were considered. It was concluded that the bare hot liner concept is not applicable to the GCFR for the following reasons:

1. With the GCFR cold leg (core inlet) gas temperature of 315.5°C (600°F) on the bare hot liner, the concrete temperature exceeds the ASME Code, Section III, Division 2, requirements of 65.6°C (150°F).
2. Alternative methods of alleviating the higher concrete temperature requirement (reducing the core inlet gas temperature at the liner face or reducing the heat transfer to the liner through a stagnant gas condition adjacent to the liner) compromise the

system design. Reduction of the core inlet temperature reduces steam generator performance and plant efficiency, and introduction of a flow shield to effect a stagnant gas condition reduces accessibility to the liner.

3. The bare hot liner design involves a complex and costly construction sequence including tensioning, relaxing, and retensioning of the prestressing system, which is not compatible with the circumferential wire-winding system.
4. The thermal cycling conditions for a hot liner and anchor studs are severe, and the consequent stresses are unacceptably high.
5. Severe liner stresses are expected to occur in areas of discontinuity, such as the cavity corners and liner/duct intersections, and at points of attachment of major components within the reactor core cavity. Such local high-stress regions, which are the most likely places for liner cracks to originate and which are generally well understood for the cold liner, are more critical and less well understood in the case of the hot liner.
6. The hot liner is much thinner than the cold liner and is thus more prone to leakage and damage during construction.

Improving the integrity of the PCRVR liner by providing for inspection and repair using a hot liner is infeasible for the temperature conditions presently specified for the GCFR. Efforts to accommodate the limitations of the hot liner design have resulted in NSSS design complexity and plant performance penalties which outweigh the potential benefits. In addition, there is the possibility of an extensive development program and the likelihood of higher construction costs. Thus, the PCRVR design incorporating the cold liner design presently specified for the GCFR is judged to be the most viable concept and should be retained.

16.3. UP-FLOW/DOWN-FLOW CORE

The incentives for considering the up-flow core as an alternative to the down-flow core of the present reference design include (1) compatibility with natural circulation (for improved safety); (2) potential for simplified refueling from above, including application of well established above-core refueling equipment and procedures; (3) possible application of water flooding; (4) elimination of bottom refueling penetrations; and (5) easier inspection and servicing of reactor internals. These concepts have been successfully applied to LWRs and LMFBRS.

The study concentrated principally on the up-flow core, since much work has already been done on the down-flow core for the reference design. The significant advantages and disadvantages applicable to each concept are listed in Tables 16-1 and 16-2. The study verified the basic feasibility of both concepts; however, more work is required to clarify the design optimization and cost before a final selection can be made. Investigations are still required on the effect of cladding melting and melting propagation in both concepts, the possible need for core restraint and assembly spacers in the up-flow core design, and refueling methods.

If PLOF were to occur in a down-flow core, it could be of greater consequence than if it occurred in an up-flow core, unless coherent fallaway from the control rods could be prevented. In the up-flow, core natural circulation would inhibit initiation of the accident. However, if fuel melting were to occur, recriticality would be likely to follow in an up-flow core design.

The safety assessment of severe accident conditions (i.e., flow blockage, loss of flow, and sudden reactivity insertions) and their subsequent core damage is not yet sufficiently advanced to identify clearly the advantages or disadvantages of an up-flow versus a down-flow core. The down-flow core seems to have identifiable advantages with respect to local flow blockages and unprotected accidents. However, a decisive advantage can only be claimed if incremental fuel removal from the core (for example,

TABLE 16-1
UP-FLOW CORE

Advantages

Compatible with top or side refueling (new refueling system required)

Enhances natural convection core cooling (possible safety advantage)

Does not require penetrations in the bottom of the vessel (simplified core catcher design)

A pneumatic fuel assembly hold-down system is applicable

Reduced PCRV height-to-diameter ratio (seismic advantage)

Thermocouples for core outlet instrumentation may be installed from above without passing through core assemblies

Grid plate can be supported from side or below (possible fabrication advantage with the latter)

Core assembly fallaway is not possible

Disadvantages

Need for increased upper shielding and insulation to protect closure, control rod drives, and instrumentation

Control rod drive mechanism and instrumentation leads located in high-temperature, high-flux region

More complex grid support system and grid plate design

Need for lateral core restraint system to provide lateral positioning of control rod drive assemblies and core distortion reactivity control

Sweep-out of molten fuel assembly material less likely

Possibility of need for a core spacer grid to prevent movement and vibration of core

Core is radially and axially larger to accommodate inner radial shield assemblies; fuel assembly is longer to accommodate control rod within assembly

Assembly interaction forces may be higher owing to lateral restraint system and may impose greater load capacity requirements on refueling equipment

Possibility of accidental dropping of a fuel assembly onto top of core

TABLE 16-2
DOWN-FLOW CORE

Advantages

Rigid locking of fuel assemblies in grid plate assures positive control rod shutdown operation (a)

Control rod drive mechanisms and thermocouple leads are in cool-end environment

Removal of molten fuel debris from faulted core assemblies more likely because of combined gravity and flow effects

Accidentally dropped fuel assembly caught by lower shielding (adequate cooling available)

Fuel assembly bowing produces negative reactivity effects under all core heat-up conditions

Disadvantages

Bottom or side refueling only

Incompatible with natural convection core cooling

PCRV height-to-diameter ratio larger than that for up-flow core configuration owing to space required above grid plate (seismic disadvantage)

Thermocouple leads have to penetrate fuel assemblies through highest flux region to outlet nozzle; measurements may be unreliable

Bottom refueling for down-flow core requires large clearance below PCRV

Grid plate supported at its edge (weight and stiffness disadvantage)

(a) No seismic shear problem between the control rod mechanism and the control rod core assemblies.

by individual duct fallaway) can be demonstrated. This has not yet been fully established. Since the analysis for core disruptive accidents is incomplete, any comparison of the safety aspects of the up-flow and down-flow cores must be considered preliminary. More definitive study of the influence of core flow direction on the safety and reliability of the plant is necessary.

16.4. CACS AND NATURAL CONVECTION COOLING

An independent CACS is required as a backup for the main loop cooling system. Therefore, the feasibility of using natural convection to remove residual heat in the event of a total loss of forced cooling was investigated. Total loss of forced cooling is regarded as a low-probability event, but it has a higher probability of occurrence than a DBDA. Cooling by natural convection is a desirable feature, although it is not expected to replace the CACS. With an up-flow core and some design changes to the CACS, natural convection appears to be feasible for the pressurized condition.

There is no possibility of achieving natural circulation for the DBDA condition. The natural convection system would have to be designed (assuming steam-driven main circulators) to reject 40 MW(t) of power (4.8% of full power) using two out of three CACS loops. The circulation height between the core and the CAHE is approximately 8.4 m (27.5 ft), with ~ 21 m (~ 70 ft) on the water side and ~ 15 m (~ 50 ft) between the heat source and heat sink on the air side. This corresponds to a maximum fuel rod cladding temperature of 1065°C (1950°F) and a core outlet temperature of 760°C (1400°F). Performance margins for this preliminary design analysis are low but could be improved by either the use of electric main circulator drives to enhance the coast-down characteristics or the addition of short-time battery power for initiating and accelerating water and air circulation. Uncertainties which still need to be investigated include heat transfer and friction factors under natural convection conditions; location and operation of the main and auxiliary loop isolation valves (which are required to operate quickly and reliably and, preferably, without external power); licensability of

such a system without prior demonstration; and additional cost of CACS design and development with forced and natural convection capabilities. The latter two uncertainties are inseparably linked to the major design and development effort associated with the up-flow core concept and an entirely new fuel handling system.

16.5. CIRCULAR FUEL ASSEMBLIES

The circular assembly is a viable alternative or backup design. The creep-induced assembly duct dilation and associated performance penalties can be largely eliminated by changing the duct design from a hexagonal to a circular section. Penalties in the area of thermohydraulic and nuclear performance will be minimized or eliminated by taking advantage of the structural and design characteristics associated with the circular duct.

Mechanical interference between circular fuel assemblies as a result of bowing appears less likely because of the reduced swelling-related duct distortion. After duct failure, drop-out of the lower duct is expected to be more likely in a circular assembly and may prevent recriticality owing to slumping of the fuel into a more compact configuration.

16.6. SHIELDING AND FLOODING

A shielding and activation analysis was made to determine the activation level of the reactor internals and the possibility of in-service inspection, maintenance, and repair. The effects of flooding the reactor cavity with water after defueling were also investigated. This analysis was made for the reference, up-flow, and down-flow core designs. The gamma dose levels of the GCFR on the core side of the radial shield after core removal and approximately three days after shutdown are generally too high for any human activities. Therefore, even if the cavity were flooded, repair by remote equipment will be required.

16.7. LINER AND THERMAL BARRIER INSPECTION

The likelihood of liner leakage is remote since there is no credible mechanism by which a crack would initiate or propagate after the PCRV is leak-tested. This is true for the HTGR as well as the GCFR. The in-service inspection criteria established in the ASME Code (proposed Section XI, Division 2) do not require in-service inspection of the PCRV liner. Only the closures and local regions of the penetrations must be inspected, since they must resist primary coolant pressures and perform as LWR pressure vessels rather than as a liner. Therefore, special provisions in the PCRV design for inclusion of features permitting easy accessibility for liner repair are neither necessary nor warranted.

It is, however, useful to know the consequences of minor or postulated excessive liner leakage and the courses of action, if any, necessary to mitigate the effects of such leakage. It is recommended that the effects of radiation release on plant availability be investigated along with the effects of pressurized gas on the concrete side of the liner, the structural concrete, and the PCRV prestress system. The economic consequences of excessive leakage to the owner, who is required to supply makeup helium, should be assessed, and the need to locate a liner leak and determine whether to collect the effluent or repair the liner must also be studied. If collection and/or repair work is found to be necessary, methods of accomplishing this task should be defined, and its impact on plant design evaluated.

16.8. WET REFUELING

The concept of wet refueling, or flooding, of the reactor cavity is analogous to that used in LWRs, and previous experience could be applied to the GCFR. Flooding cannot seriously be considered to be applicable to a design in which the thermal barrier is of the fibrous material used in the GCFR; it requires either an all-metal thermal barrier or a bare liner. Two concepts have been considered for controlling reactivity when introducing water into the reactor cavity.

The first concept introduces boronated water in which the H_3BO_3 has been enriched by the boron isotope B-10. A major disadvantage of this concept is the high cost and low availability of B-10-enriched boron; the quantity required for GCFR flooding is several times the U.S. annual production. The second concept incorporates the poison into the fuel in the form of gadolinium oxide. This would substantially reduce the cost of the poison, but the poison in the fuel would displace some of the fertile material, resulting in a large reduction in breeding ratio.

Underwater refueling requires a change from gas to liquid cooling of the core and core assemblies, and a rapid flow of water must flood the cavity to cool the core effectively during flooding. Once the operation has started, it must be completed without interruption, because when the water level has reached the core, it will restrict the gas flow to the core. Provision must also be made to remove the helium gas at essentially the same rate as the water in-flow. Intentional or accidental flooding of other sections of the primary and auxiliary loop with the consequent difficulties of dry-out and the possibility of residual boron oxide deposit could cause corrosion. Similar problems are possible in the event of inadvertent influx of water into the core PES and the fuel assemblies. Restrictions on the type of vessel and insulation which can be used together with the system and operational problems of flooding have ruled out this concept for the GCFR.

16.9. REFUELING

Several refueling methods were investigated for application to the up-flow core, including some which introduce a structure above the core to guide the control rod drive mechanism. One method considered was a combined system involving servicing from the top and the side. This concept was previously investigated for the reference design down-flow core and was applied to the alternative design, but was not pursued further because it involved simultaneous activities from two locations and resulted in a generally complex system. Another concept examined was based on a fixed closure, with the fuel handling machine being inserted through the control rod penetration

after removal of the control rod drives. The fuel handling machine required a grapple head which could extend to the surrounding six assemblies. This system is relatively simple but has the disadvantage of having to be relocated in each of the control rod penetrations to cover the core.

A third concept employed a single rotating plug which contained the control rod penetrations with two additional radial penetrations in the blanket and shield regions. By rotation of the plug and sequential use of one line of penetrations and a grapple head to extend to the surrounding six assemblies in each zone, the core blanket and inner shield can be covered. Either of the latter two concepts is feasible, but it is recommended that they be examined in greater detail to establish which would be most applicable to the up-flow core design.

16.10. POSTACCIDENT FUEL CONTAINMENT

At present there is no requirement for a core catcher in the GCFR design; however, design and analysis is proceeding on core catcher options applicable to the reference design in case such a requirement is specified. The basic design with minor modifications will be applicable to the alternative designs. Elimination of the refueling penetration in the bottom of the PCRV would be an improvement on two counts: (1) there would be no gap (around the plug) through which molten fuel and debris could exit from the primary zone, and (2) the wider dispersal of the molten debris would reduce the possibility of recriticality. Provisions should be made in the design to accommodate a core catcher.

16.11. ALTERNATIVE FUEL CYCLES

The GCFR can operate on virtually any fuel cycle suggested for fast breeder reactors. In addition, the GCFR has significant fuel efficiency advantages for cycles which employ U-238 and/or Pu-239. In fuel cycle strategies which restrict plutonium utilization, the GCFR is the most effective fuel factory. The GCFR can also be used to supply fuel for

advanced converter reactors to provide total fuel systems which offer reduced proliferation risk and an adequate and flexible nuclear energy supply for the twenty-first century.

16.12. PREOPERATIONAL TESTING

The necessity for preoperational non-nuclear flow testing of the primary coolant circuit and its flow, vibration, and acoustic interaction with NSSS components was explored. It was concluded that a program including full mass flow preoperational testing of each loop followed by partial flow ($\sim 30\%$) three-loop testing will satisfy the requirements for checking the system for flow-induced and acoustically induced vibration. It is recommended that this minimum procedure be adopted as a requirement for the demonstration plant. Use of electric drive circulators would allow simultaneous full mass flow testing of all three loops.

16.13. USE OF MAIN LOOPS FOR AUXILIARY CORE COOLING

A preliminary study was made of configuration design variations using main loop cooling for all service conditions including postulated accidents. These concepts offer the possibility of (1) eliminating the separate vessels for containing the core auxiliary heat exchanger and circulators and (2) improving the flow path to facilitate natural circulation.

Two versions were considered. In the first, the reactor heat can be rejected only through the steam generators. In the second, the heat can be rejected via an auxiliary heat exchanger arranged in series or in parallel with the steam generator and incorporated in the steam generator. Each main loop is provided with two 50% capacity circulators powered from independent sources. However, further analysis of the design, safety, and licensing aspects of these concepts is needed before definite conclusions can be reached.

REFERENCE

- 16-1. "GCFR Alternative Design Study," DOE Report GA-A14856, General Atomic Company, February 1978.

17. GCFR ALTERNATE FUEL CYCLES (189a No. 00761)

17.1. CORE DESIGN PROGRAM OUTLINE

The general outline of the GCFR core design efforts for alternate fuels is presented in Table 17-1. During FY-77 the preliminary analysis and evaluation phase was completed (Refs. 17-1, 17-2). The core design and materials optimization phase is now under way and is discussed below. Related efforts on fuel strategies are also summarized.

17.2. INTEGRATED REACTOR STRATEGY STUDIES

The mass flow information and other GCFR characteristics determined in the preliminary evaluation phase have been used in subsequent reactor fuel strategy studies to delineate further the role of the breeder in various fuel cycles. To accomplish this, similar mass flow information was required for various types of advanced converter reactors (ACRs). The strategy studies completed in the preliminary evaluation phase (Ref. 17-1) qualified the maximum energy available as a function of time given various fuel cycle scenarios. These studies suggest that an integrated fast breeder reactor (FBR)/ACR system could meet moderate nuclear energy demand conditions while greatly reducing the number of plutonium-utilizing facilities and virtually eliminating the need for transport of plutonium-bearing fresh fuel. Therefore, studies were initiated to define a small number of potentially attractive fuel strategies which could represent a compromise between proliferation concerns and energy needs.

In the past, national energy plans assumed that long-range fuel strategies consisted simply of plutonium/uranium breeders started up from the plutonium discharge of LWRs. Revised estimates of nuclear markets, uranium supply, and costs combined with increased concern about the risks

TABLE 17-1
GCFR ALTERNATE FUEL CYCLE STUDY: CORE DESIGN

Preliminary analysis and evaluation

- Identify cycles of interest
- Determine GCFR characteristics, typical design
- Evaluate potential strategies
- Identify fuel cycles of greatest interest

Core design and material optimization

- Identify major parameters for optimization
- Identify potentially attractive materials
- Optimize design for reduced number of fuel cycles
- Reevaluate potential fuel strategies
- Perform preliminary evaluation of safety parameters

Fuel cycle independent core design

- Review fuel cycle optimized core design
- Identify areas of compromise
- Evaluate performance of each cycle
- Establish design parameters for fuel cycle independent core design

of proliferation require that alternative strategies be considered. A number of scenarios have been developed for the next 50 to 60 years of the U.S. nuclear power industry. These scenarios represent attempts to balance proliferation concerns, resource (ore) requirements, economics, and the need for preservation of a long-term nuclear energy option. Various combinations of ACRs and FBRs can produce systems which have the flexibility to allow these major concerns to be balanced.

The assumptions which provide the basis for the scenarios are as follows:

1. Nuclear energy growth. The growth model is essentially a moderate case (Ref. 17-3). It reaches an installed capacity of 400 GW(e) in the year 2000 and increases at the rate of 15 GW(e)/yr thereafter. After 2040, the 1000-GW(e) capacity is maintained for as long as the available resources allow the system to operate. The capacity could be expanded with additional resources.
2. Uranium resource availability and cost. About 3.5×10^6 short tons of U_3O_8 are economically available, with tails enrichment of 0.15% over the whole time period. The increased difficulty in mining larger amounts of ore is reflected in higher ore costs. Reference 17-4 was used for cost estimates.
3. Reactor introduction conditions. (a) LWRs operate alone until 1990 without recycle. (b) ACRs (i.e., HTGR, CANDU, spectral shift reactors) start operating in 1990 on the medium-enriched-uranium (MEU/Th) fuel cycle without recycle. (c) FBRs start operating in 2000 based on the availability of reprocessing of LWR fuel to obtain plutonium before 2000; FBRs operated in secured areas. (d) After 2005, highly enriched U-233 can be used to feed ACRs outside secured areas. In two scenarios, high-enriched U-235 is also available.

4. Reactor mass flows. The GCFR and HTGR mass flows represent FBRs and ACRs, respectively. All reactors operate at a 70% capacity factor.

These assumptions were used for modeling various reactor fuel systems. These systems were evaluated with regard to the four major concerns previously noted. The results for three systems are as follows:

1. LWR stowaway. The uranium supply is used to fuel LWRs, and all spent fuel is stored or disposed. With this strategy, nuclear capacity would reach 600 GW(e) in 2010 and decline rapidly thereafter. A total of $\sim 15,000$ GW(e)-years of nuclear power could be generated prior to exhausting the U_3O_8 supply. Nuclear power would not represent a long-term energy option, and economic costs would be significantly higher than any other strategy.
2. ACRs. ACRs are assumed to replace LWRs in the marketplace around 1995. Figure 17-1 illustrates the energy capacity of the industry if the ACRs are restricted to MEU fuel cycles. With recycle, $\sim 26,000$ GW(e)-years would be available. Without recycle, the energy available would be only slightly higher than that available from the LWR stowaway scenario. If high-enriched uranium (with its attendant proliferation concerns) is used in ACRs, nuclear capacity could grow to ~ 1000 GW(e) in 2050 before fuel shortages begin to restrict capacity. Although such a scenario could provide $\sim 44,000$ GW(e)-years of nuclear energy, it would effectively foreclose any long-term option by consuming all the available fissile material.
3. FBR/ACR symbiosis. If FBRs and ACRs are introduced around 2000, symbiotic systems can be employed to give much of the fuel resource advantage of FBRs without requiring large numbers of FBRs. Figure 17-2 illustrates one such scenario in which a minimum number of breeders are utilized to support ACRs. With

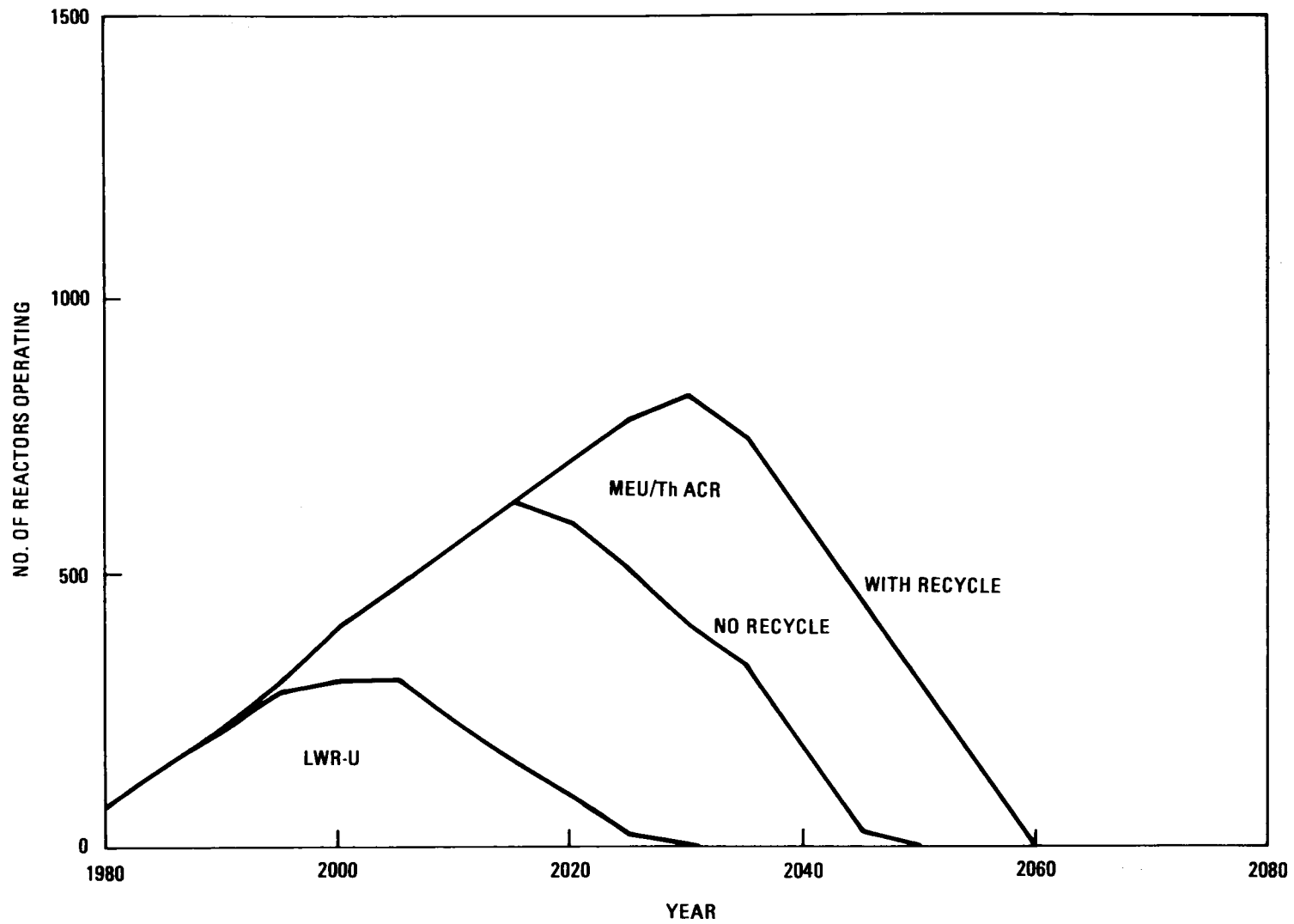


Fig. 17-1. Number of reactors in operation

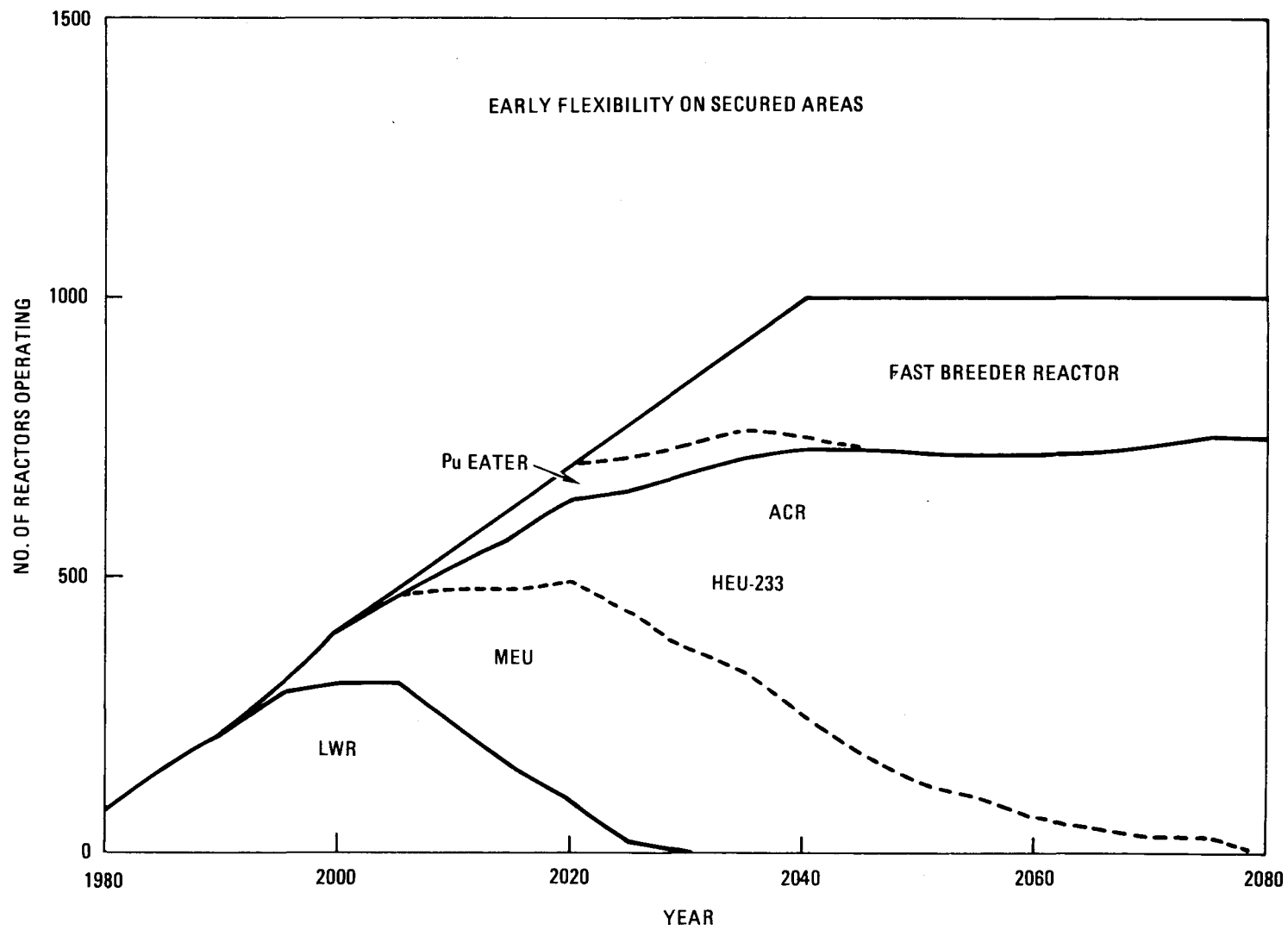


Fig. 17-2. Long-term reactor system

this type of strategy, nuclear energy would be available for thousands of years from the 3.5×10^6 short tons of U_3O_8 . If the ore resource is determined to be smaller, a higher fraction of FBRs can provide additional flexibility. The use of high-enriched uranium in ACRs requires less U_3O_8 and fewer FBRs.

If nuclear power is to be a long-term energy option with sufficient flexibility to deal with the concerns raised, then

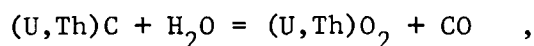
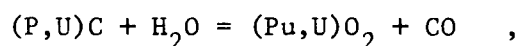
1. Reprocessing must be available by ~2000.
2. An ACR will be necessary to extend current resources and to supply energy in a breeder-based system.
3. The FBR will be required early in the twenty-first century to provide an assured fuel supply while eliminating the need for use of potentially dangerous materials (i.e., those that can be used to make weapons) outside secured areas.

A more detailed description of the continuing reactor strategy studies is presented in Ref. 17-5.

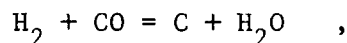
17.3. ALTERNATE FUEL CYCLE MATERIALS

The objective of this subtask is to provide material guidance and assessment to the alternate fuel cycle core design and management studies. Assessment of materials for the GCFR is somewhat different than that for alternate cycle materials for the LMFBR because of the use of vented fuel. The use of vented fuel in a GCFR implies at least the potential for primary coolant helium containing H_2 and H_2O impurities having access to the fuel. This access, through the vent hole during inhalation, is believed to be small and tolerable for oxide-based FBR fuels, although there may be some effect on the fuel stoichiometry. However, for carbide and nitride fuels, which are extremely sensitive to moisture, the effect may not be tolerable. A somewhat lesser, although important, effect is likely for metallic fuels.

Physicochemical data on oxide-based alternate fuel cycle materials is being assembled so that an assessment of the impact of venting on plutonium/thorium and the denatured uranium cycle fuels can be carried out. Calculations of the equilibria, i.e.,



need to be carried out to assess the quantity of carbide that will be converted to oxide. These equilibria will be studied along with the water gas equilibrium, i.e.,



which will be governed by the H_2/H_2O ratio in the fuel rod and the rate at which H_2 diffuses through the cladding.

17.4. CORE PHYSICS DESIGN: SENSITIVITY STUDIES

A series of studies has been initiated to determine the sensitivity of breeding ratio, specific power, doubling time, and other fuel cycle characteristics to various core design parameters. The initial studies are intended to help guide optimization of future core designs as well as provide verification of the less sophisticated nuclear model incorporated in the CALIOP code (Ref. 17-6). The specific parameter studied was fuel volume fraction, or fuel rod size. CALIOP was used to define three core configurations having a constant pressure drop and fuel rod sizes of 5.5, 7.9, and 9.9 mm. For the initial study, a plutonium/uranium core was modeled to provide a basis for verifying the CALIOP nuclear model. Table 17-2 presents a list of major core design characteristics for the rod sizes studied. Each design was then modeled by multidimensional diffusion calculations, and breeding ratio and mass flow characteristics were determined.

TABLE 17-2
CORE CHARACTERISTICS

	Rod Configuration		
	1	2	3
Fuel rod o.d. (mm)	5.5	7.9	9.9
Fuel volume fraction	0.18	0.31	0.37
Steel volume fraction	0.14	0.18	0.21
Rating (MW/kg-fissile)	1.07	0.71	0.51
Core length (cm)	121.4	110.0	110.0
Core L/D	0.31	0.24	0.21
No. assemblies	397	433	436
Cladding material	PE-16	PE-16	PE-16
Core enrichment (%)	18.0	12.6	11.5
Core diameter (cm)	391.6	458.3	523.8
Core material	(Pu/U)O ₂	(Pu/U)O ₂	(Pu/U)O ₂
Axial blanket material	UO ₂	UO ₂	UO ₂
Radial blanket material	UO ₂	UO ₂	UO ₂

Figure 17-3 illustrates the dependence of breeding ratio on fuel volume fraction for these rather conservative core designs. Figure 17-4 presents comparable information on breeding ratio and specific power in a more general format and illustrates that while breeding ratio increases monotonically with rod size, specific power decreases monotonically. The net result is that doubling time passes through a broad minima near a rod size of approximately 8 mm. The in-core conversion ratio increases rapidly with rod size, reaching 1.0 at a rod size smaller than 10 mm. If thorium blankets are used with a plutonium/uranium core, then U-233 production is maximized at a small rod size even though total fissile production is minimized. Optimization of the core design will therefore be dependent on the role of the breeder as well as the core fuel cycle employed. Comparison of CALIOP with the multidimensional analysis indicates that the one-dimensional, one-group nuclear model in CALIOP will be sufficient for much of the sensitivity analysis being done for the plutonium/thorium and low-enriched U-233/Th GCFR core designs.

17.5. NASAP DATA FOR GCFR

Immediate mass flow and alternate fuel cycle information for the GCFR was requested by DOE. The data are required for the Nonproliferation Alternative Systems Assessment Program (NASAP). Although information generated earlier in the GCFR alternate fuels program could have been used, the previous design bases were more conservative than those being used for NASAP by other FBR contractors. Therefore, an effort to provide mass flows on a basis consistent with other FBR programs was initiated. Linear heat ratings were increased and high-nickel (reduced-swelling) alloys were replaced with stainless steel. Work was aimed at providing mass flows for plutonium/uranium cores with uranium and thorium blankets as well as a plutonium/thorium core. Based on the limited sensitivity work completed, different fuel rod sizes were selected for the plutonium/uranium and plutonium/thorium cores, and mass flows were generated. Additional cycles including low-enriched U-233/Th and plutonium carbide are being calculated.

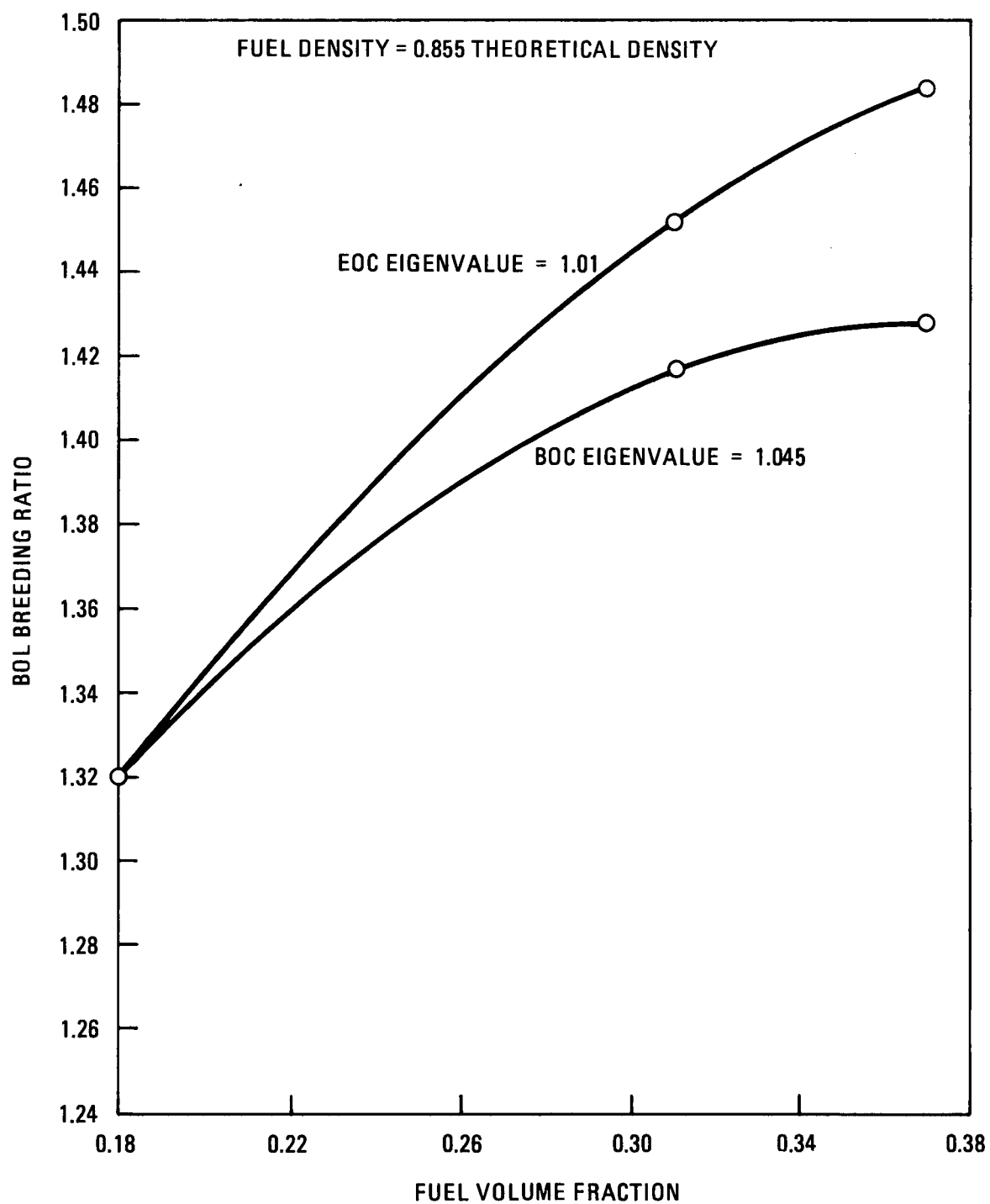


Fig. 17-3. Breeding ratio for 1500-MW(e) GCFR

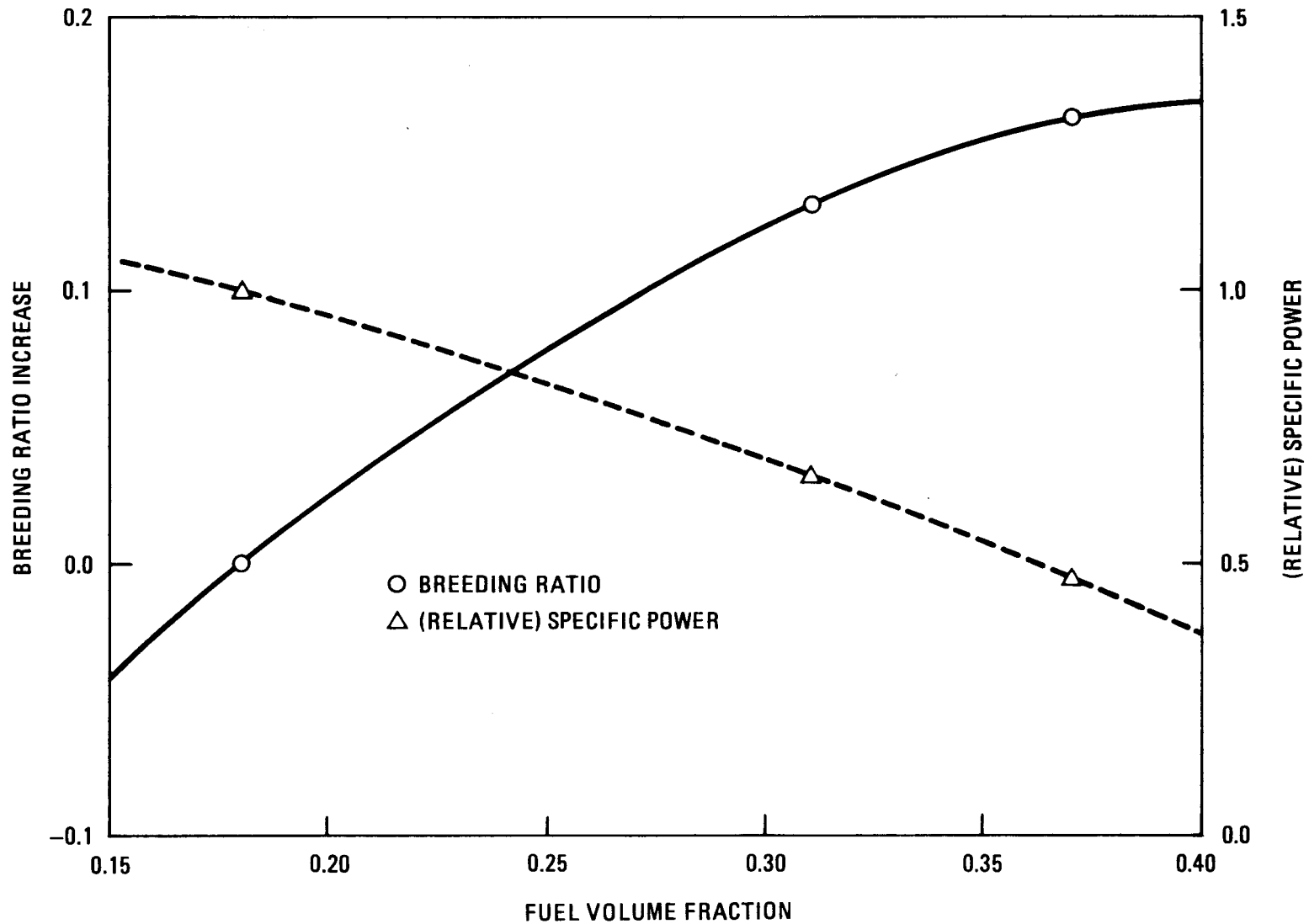


Fig. 17-4. Breeding ratio increase and (relative) specific power as a function of fuel volume fraction: large Pu/U oxide core GCFR

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