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GAS-COOLED FAST BREEDER REACTOR

**QUARTERLY PROGRESS REPORT FOR THE PERIOD
NOVEMBER 1, 1979 THROUGH JANUARY 31, 1980**

by
PROJECT STAFF

MASTER

**Prepared under
Contract DE-AT03-76SF71023
for the San Francisco Operations Office
Department of Energy**

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GENERAL ATOMIC COMPANY

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ABSTRACT

The tasks of the gas-cooled fast breeder reactor (GCFR) program that 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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1.0. NUCLEAR STEAM SUPPLY SYSTEM

1.11. REACTOR VESSEL SYSTEM

1.11.1. Prestressed Concrete Reactor Vessel

A symmetric and an asymmetric arrangement of the prestressed concrete reactor vessel (PCRVR) for a six-loop plant were compared. The PCRVR operating pressure of 11.0 MPa (1580 psig) was used, resulting in a maximum cavity pressure of 12.5 MPa (1797 psig). The PCRVRs were sized to provide adequate radial and circumferential ligaments and tendon placement. The o.d. is 41.45 m (136 ft), and the height is 39.62 m (130 ft). Figures 1-1 and 1-2 show the symmetrical and asymmetrical PCRVR arrangements, respectively. A layout study indicated that no PCRVR size reduction would be achieved by a core cavity offset for the asymmetric arrangement. On the other hand, an asymmetric arrangement of heat exchanger components may facilitate separation of safety-related piping from main steam piping.

Evaluation of the bottom and top exhaust steam generators continued, and it was recommended that the top exhaust, top-hung steam generator be adopted for the three-loop demonstration and the six-loop commercial plants. In the three-loop demonstration plant with the recommended steam generator, the support flange is located underneath the cross duct to the core auxiliary heat exchanger (CAHE). This results in a hot duct from the core to steam generator which is either inclined upwards 52 deg from the horizontal, as shown in Fig. 1-3, or has two 90-deg bends. The inclined hot duct is preferred with respect to PCRVR, liner, and thermal barrier design. (Ref. 1-1). The lower duct to the horizontal circulator can be either horizontal or inclined downwards 30 deg from the horizontal.

NORMAL OPERATING PRESSURE 110 BARS (11.0 MPa)

Fig. 1-1. Symmetric arrangement of six-loop PCR

Fig. 1-2. Asymmetric arrangement of six-loop PCRV

Fig. 1-3. PCRV for two-loop demonstration plant with top hung, top exhaust steam generator

1.11.2. Core Cavity Closure Design and Analysis

A preprocessor computer program was completed for use in the stress analysis of the core cavity closure. The closure is a deep, perforated plate. The program generates the card images for describing a three-dimensional perforated plate model in the MESH3A program (Ref. 1-2), and the MESH3A output is used as input for the THREED stress program (Ref. 1-3). The model is 30 deg in circumference and can have any number of holes on a triangular pitch. The input variables are the diameter and depth of the plate, the number of holes, the radius and pitch of the holes, the thickness of the membrane elements lining the holes, and the number of elements through the plate depth.

Comparison of the results of THREED and SAFE-2D (Ref. 1-4) plane stress analyses for the core cavity closure model subjected to a uniform sidewall pressure indicated that there are differences in calculated stresses of up to 15%. A similar comparison with the penetration liners removed from the closure showed the same difficulty. Analyses of similar but simpler geometries indicated that the 15% difference is probably caused by the difference in model stiffnesses. A more flexible SAFE-2D plane stress model with smaller elements is expected to produce results closer to those from the THREED analysis.

SAFE-2D axisymmetric and THREED analyses of the core cavity closure without penetration liners and subjected to various pressure loadings are in progress. The results will be compared to assess the accuracy of the ASME perforated plate analysis method (which uses smeared properties) applied to deep concrete closures. A reliable and efficient procedure for preliminary analysis and/or parametric study of the core cavity closure with penetration liners will be developed.

A thermal elastic analysis of a control rod drive/refueling penetration in the core cavity closure in conjunction with a previous stress analysis indicates that a thermal sleeve is required at the base of the penetrations

for an acceptable fatigue life. Elastic methods are conservative in comparison with plastic methods. However, since the design is preliminary and the thermal cycles can only be estimated, a conservative design incorporating a thermal sleeve is being adopted. If a thermal sleeve is necessary, there will be less clear space between penetrations unless the penetration diameters decrease or the pitch between penetrations increases.

1.11.3. Thermal Barrier Development

Development of the thermal barrier was separated into four tasks:

1. Studies of the effects of helium coolant impurities on metals.
2. Investigation of the effects of water exposure on fibrous ceramic materials.
3. Feasibility tests on the flooding and draining of thermal barrier assemblies.
4. Preliminary design studies and analyses for plant conceptual design.

The fourth task is primarily concerned with estimating heat loads, establishing thermal barrier thicknesses, and developing other basic design data for the various zones of the three-loop demonstration plant.

1.11.3.1. Effects of Helium Coolant Impurities on Metals. Various metals were exposed to helium coolant impurities for 500 h, and the following results were obtained:

1. Slight oxide formation was detected on the carbon steel specimens at 315°C (600°F). Heavy oxidation was observed at 538° and 593°C (1000° and 1100°F).

2. Slight oxide formation was observed on 316 stainless steel at 538°C (1000°F). Oxidation was observed on all 316 stainless steel specimens at 593° and 648°C (1100° and 1200°F).
3. There was no evidence of oxidation for Hastelloy X at 538° and 593°C (1000° and 1100°F). Slight oxidation was evident at 648°C (1200°F).
4. There was no evidence of decarburization for carbon steel at 315° and 538°C (600° and 1000°F). Decarburization was observed at 593°C (1100°F).
5. There was no evidence of decarburization for 316 stainless steel and Hastelloy X at temperatures up to 648°C (1200°F).

Specimens exposed for 2000 h are being evaluated.

1.11.3.2. Effects of Water on Fibrous Ceramic Materials. The ceramic fiber blankets in the thermal barrier will be immersed in water for long periods of time during flooding of the GCFR cavity for major maintenance/repair operations. Hence, the effects of flooding on the resiliency of the ceramic fiber blankets must be determined. Two types of blankets are being tested: Kaowool and Saffil. The blankets are loaded in air to approximately 6.9 kPa (1.0 psi), and the load is monitored as the blankets are soaked and dried out. Previous work has shown little change in the load upon addition of water to either Kaowool or Saffil. However, when the water is drained off, the surface tension of the residual water between the fibers decreases the load by ~50%. When the blankets are dried, the load slowly increases to ~80% of what it was before soaking because of the gradual relief of water surface tension during evaporation.

A stack of four 25.4 x 254 x 254 mm (1 x 10 x 10 in.) Kaowool blankets was tested for three cycles. This Kaowool was "needled" in contrast to the "non-needled" Kaowool used in previous tests. When water was added to the needled Kaowool, the load dropped ~20% from the air load value, whereas the

non-needed Kaowool had little change in load when water was added. The load of needed Kaowool dropped to ~50% of the air load value and then came back to within 80% of the air value upon drying. Thus, the overall behavior of needed and non-needed Kaowool was essentially the same except that the permanent 20% loss of load in the non-needed Kaowool was lost during water draining and in the needed Kaowool, during water addition. During the two subsequent soaking cycles, the load on the needed Kaowool blankets dropped a little further each cycle. By the third cycle the load decreased when the water was drained to ~40% of the original air load. Upon drying out, the load recovered to only 73% of the original air load compared with 80% for the first cycle. Cyclic soaking of Saffil is under way.

1.11.3.3. Feasibility Tests of Floodable Thermal Barrier. Encapsulated specimens of saturated Kaowool and Saffil insulation were dried by two methods: (1) heating to 204°C (400°F) or (2) heating to 66°C (150°F) with evacuation to 6.9 kPa (1.0 psia). The results of this series of six insulation drying tests are as follows:

1. Either method of drying satisfactorily removed all the water from the encapsulated fibrous insulation, which is typical of the thermal barrier.
2. The water was boiled off and vented more quickly with the first method (24 h) than the second (3 to 4 days). The drying-out period for the latter method can probably be significantly shortened by cycling the pressure several times, i.e., pumping.
3. There was no significant difference in the drying rates of the Kaowool and Saffil.
4. Post-test disassembly of the housing showed that Kaowool and Saffil adhered to all the adjacent stainless steel surfaces (Fig. 1-4). This could become a significant factor in designing a replaceable floodable thermal barrier.

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Fig. 1-4. Dry Kaowool test 2a

Insulation blanket mechanical stability tests were performed to determine the effects of flooding and draining on the dimensional and mechanical stability of the thermal barrier assemblies. The transparent test housing simulates a cavity vertical wall and is 1.22 x 1.83 m high (4 x 6 ft) (Fig. 1-5). Colored deionized water was used so that its progression through the insulation could be observed.

Two tests were completed. The first test used four 25.4 mm (1.0 in.) thick Kaowool insulation blankets compressed to 81.3 mm (3.2 in.), which exerted a compressive force of approximately 6.9 kPa (1.0 psi) in the dry state. Two cycles of flooding (24-h submergence) were followed by draining until dripping ceased (up to 15 days). There was no indication of slumping of insulation or movement of any components throughout the test. After draining was terminated, 51.25 kg (113 lb) of water remained in the insulation; the bulk of the water was in the heavily saturated lower blanket. Figure 1-6 shows the vertical rate of change of the density of the water trapped in the insulation.

The second test used low-density Saffil mat insulation compressed to 106.7 mm (4.20 in.) and exerting a lower compressive force of 3.5 kPa (0.5 psi). This simulates a possible late-in-life reactor condition. This test remained stable through two cycles of flooding and draining. Post-test examination of the Saffil blankets verified that the resiliency of the saturated blanket deteriorated to approximately 0.83 kPa (0.12 psi), but it gradually recovered as it dried.

1.12. REACTIVITY CONTROL AND INSTRUMENTATION MECHANISMS

Development of a conceptual design for the shutdown drive mechanism has been temporarily halted pending definition of an acceptable degree of diversity between it and the primary control rod system. 10CFR50 states that two different design principles be used for the primary and secondary reactivity control systems.

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Fig. 1-5. Transparent test housing, Kaowool

Fig. 1-6. Water content of insulation after draining

1.13. FUEL HANDLING SYSTEM

Three general problem areas associated with adapting the demonstration plant refueling concept to a large commercial plant are being studied:

1. Refueling time.
2. Balance of plant (BOP) and PCRV interfacing.
3. Seismic impact.

Refueling time estimates were prepared for six variations of the basic system for the demonstration plant.

1.17. REACTOR INTERNALS SYSTEM

1.17.1. Core Support Structure

The purpose of this subtask is to assure the availability of analytical methods to assess the structural integrity of the GCFR core support structure and the materials mechanical behavior under all anticipated operational and safety-related loading conditions.

1.17.1.1. Thermal Analysis. The axisymmetric thermal expansion load on the core barrel due to bowing of the fuel, shield, and blanket assemblies was calculated. The estimate includes creep thermal gradients and stiffness adjustment factor.

1.17.1.2. Stress Analysis. A preliminary stress analysis was performed for the three-loop demonstration plant to (1) determine the effect of the core support components on the core support structure (under mechanical and thermal loads); (2) reduce the thickness of the grid plate and the supporting cylinders; and (3) provide information for a finite element model stress and dynamic analysis. The preliminary results indicate that the stresses and fatigue usage factors in the core support structure satisfy ASME Section III, Subsection NG, requirements. The fatigue usage factor for the support

flange bolts is marginal, but more precise thermal cyclic loads for the core support structure are needed.

The configuration of the thermal sleeve forging has been revised to provide additional space for the lower plenum shield.

1.17.1.3. Pressure Equalization System Matrix Connection Plate. Figure 1-7 shows the layout of the pressure equalization system (PES) plate atop the grid plate and the in-cavity portion of the tubing connecting the PES plate with the monitor stations. The tubing is routed downward through holes in the grid plate, and six holes, each with a diameter of 100 mm (4 in.), are provided at the edge of the grid plate adjacent to each of the hexagonal core flats. In this location, the effect of the holes on grid plate stiffness is trivial. The six holes provide ample space for routing all interconnecting tubing. Each cluster of tubing is routed from its connection point at the base of the grid plate, under the core support structure, to a liner penetration. Routing the tubing below the core support structure affords greater protection and a cooler environment.

1.17.2. Reactor Shielding Assemblies

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

1.17.2.1. Thermal Analysis. Preliminary thermal analyses have indicated that the I-beam support struts for the upper radial shielding are acceptable for normal and upset conditions. Liner and concrete temperatures adjacent to the support and stresses in the I-beam were acceptable.

Fig. 1-7. Plate installation, matrix connection

1.17.2.2. Stress Analysis. Analysis of the upper plenum shielding support structure showed that the stresses meet the ASME code requirements. Code case 1592 was used to determine the allowable stresses in the structural components at elevated temperatures. The shielding support structure is designed to withstand (1) a sustained load at 565°C (1050°F), (2) a seismic plus a sustained load at 565°C (1050°F), and (3) installation loads at room temperature.

A stress analysis of the radial shield support structure (Figs. 1-8, 1-9) indicated that these stresses meet ASME code requirements. The temperature in the radial shield support structure is substantially lower than that in the upper plenum shielding support structure. The design temperature used for the radial shield support structure is 455°C (850°F).

1.17.2.3. Shielding Concepts. Figures 1-10 and 1-11 show the recommended concepts (chevron design) for the upper and lower plenum shield structures. Figure 1-12 shows the closure plug for the upper plenum.

1.21. PRIMARY COOLING SYSTEM

1.21.1. Main Helium Circulator, Valve and Service System

The purpose of this subtask is to develop the helium circulator, its service system, and the main loop isolation valve and to demonstrate the performance and reliability of the circulator by testing under anticipated operating conditions. The overall objective for FY-80 is to complete conceptual design and performance analyses of the reference design circulator and to initiate component test programs for overall circulator assembly, compressor machine assembly (including bearings, shaft, seals and compressor wheel), main loop isolation valve, circulator scale-model air test rig, and bearing and pump test rig.

Fig. 1-8. Plan view of radial shield

Fig. 1-9. Elevation cross section of radial shield

Fig. 1-10. Reference radial shielding design (chevron shape), lower plenum

Fig. 1-11. Reference radial shielding design (chevron shape), upper plenum

Fig. 1-12. Upper plenum shield (closure)

1.21.1.1. Main Circulator Design. Horizontal installation of the main electric drive circulators in the bottom of the PCRV was chosen, considering the overall plant layout. The bearing test rig, which is already being manufactured, covers the range of powers and speeds expected for the demonstration plant. Model tests which are generic to a centrifugal flow compressor and pipe diffuser arrangement as well as an inlet duct containing a self-actuating butterfly valve in a horizontal position (which uses different size counterweights to operate the valve blades and a nonmechanical override system) are planned.

1.21.1.2. Aerodynamic Design and Analysis. New design conditions were generated for the main circulator because of the increase of reactor helium maximum pressure to 10.5 MPa (1522.9 psia). Table 1-1 shows the performance margins, which are defined in Fig. 1-13. The main circulator design allows for an increase in primary coolant system flow resistance, i.e., a 5% upward movement of the pressure versus flow curve. The design must also ensure that the circulator can produce a mass flow rate which is higher than the design point flow rate for the increased flow resistance. As indicated in Fig. 1-13, this entails running the circulator at an increased rotational speed.

The power required in the overload condition is 30% higher than that required in the design point condition, based on the assumptions that (1) the flow resistance of a fixed-geometry duct system varies as the square of the flow rate and (2) the power required to circulate the flow is proportional to the product of the flow resistance (pressure drop) and the mass flow rate.

A study was made to determine a design point speed that would meet overload conditions at 3000 rpm. This study assumed that the drive motor speed of 3000 rpm would not be exceeded and used the generalized nondimensional off-design characteristics of the centrifugal compressor shown in Fig. 1-14. If the performance parameters defined in Fig. 1-14 are used, the

TABLE 1-1
MAIN CIRCULATOR OPERATING CONDITIONS

	Design Point	Overload
Outlet pressure [MPa (psia)]	10.5 (1522.9)	10.5 (1522.9)
Inlet pressure [MPa (psia)]	10.26 (1487.7)	10.2 (1480.4)
Pressure rise [MPa (psi)]	0.24 (35.2)	0.293 (42.50)
Mass flow rate provided to loop [kg/s (lb/s)]	448.7 (990)	480.2 (1059.3)
Circulator mass flow rate (including 2.5% leakage) [kg/s (lb/s)]	459.9 (1014.5)	492.1 (1085.5)

Fig. 1-13. Circulator performance margins

Fig. 1-14. Generalized compressor performance

performance of a centrifugal compressor with a vaned or a pipe diffuser at speeds between 50% and 110% of the design speed can be represented by a single curve. Since all the design parameters including speed were known for the overload 3000 rpm condition, the values of ψ and ϕ were calculated and the design speed of the circulator was determined to be 2730 rpm. The principal dimensions of the compressor can be found in Reference 1-5. The performance prediction program PREDM (Ref. 1-6) was used for parametric studies to determine the axial length of the rotor and the number of rotor blades. Table 1-2 gives the results of the studies, and Fig. 1-15 shows the rotor profile.

The resistance of a shut-down GCFR circulator to flow in the reverse direction caused by a main loop shutoff valve being stuck open was determined. To make the results typical of a number of possible designs, the compressor was assumed to have a pipe diffuser, full impeller vanes with a radially oriented exit, and a full inducer section. To allow for small variations in the design point parameters of the circulator, the pressure drop was represented by the following normalized form for flow of pure helium:

$$\Delta P = K \frac{T}{P} \left(\frac{P_{DES}}{T_{DES}} \right)^2 \left(\frac{M_{BF}}{M_{DES}} \right)^2 ,$$

where M_{BF} = mass flow rate of backflow,

M_{DES} = design point mass flow rate,

P = pressure of helium entering compressor in backflow,

T = temperature of helium entering compressor in backflow,

P_{DES} = design point circulator discharge pressure,

T_{DES} = design point circulator discharge temperature.

TABLE 1-2
MAIN CIRCULATOR ROTOR DIMENSIONS

Tip diameter [m (in.)]	1.397 (55.00)
Eye diameter [m (in.)]	0.994 (39.12)
Hub diameter [m (in.)]	0.397 (15.63)
Axial length [m (in.)]	0.3048 (12.00)
Blade exit width [m (in.)]	0.141 (5.55)
Blade exit angle [deg (from radial)]	0.0
Blade shroud inlet angle [deg (from axial)]	47.2
Blade hub inlet angle [deg (from axial)]	46.8
Number of blades	19

Fig. 1-15. Circulator rotor profile

The constant K takes on one of two values, depending on the system of units used:

<u>Unit of Pressure</u>	<u>Unit of ΔP</u>	<u>Unit of Temperature</u>	<u>Value of K</u>
MPa	Pa	K	11,811
psia	psi	K	21.26

1.21.1.3. Rotor Dynamics, Shaft and Bearings. The bearing geometries were optimized using the JLBear code to provide the stiffnesses necessary for avoiding flexural resonance. The 260-mm (10.25-in.) diameter bearing stiffness is 9.74×10^8 N/m (5.56×10^6 lb/in.); the 215-mm (8.5-in.) diameter bearing stiffness is 5.95×10^8 N/m (3.4×10^6 lb/in.).

The torsional natural frequency of the combined electric motor and helium compressor was within the design speed range for the circulator. This condition is generally to be avoided. A more detailed evaluation of the compressor impeller (Fig. 1-15) resulted in an 18% improvement in torsional natural frequency for the helium compressor only; however, the change for the combined electric motor and helium compressor was only 1%.

1.21.1.4. Bearing Test Rig. More than 45 detailed manufacturing drawings for various components of the bearing test rig have been completed, and most components have been purchased.

1.21.1.5. Pony Drive. Replacement of the electric pony motor with a hydraulic turbine was investigated. The hydraulic turbine is compact and eliminates the common circulator drive mode, i.e., the electric motor. Figure 1-16 shows a preliminary design of an auxiliary water turbine drive for 750 (kW) at 2996 rpm. The turbine is of the full admission impulse type, and the flow requirements are $0.137 \text{ m}^3/\text{s}$ ($4.85 \text{ ft}^3/\text{s}$) at 9900 kPa (1436 psi). The wheel diameter is 300 mm (12 in.), and the blade height is 7.5 mm (0.3 in.). The turbine would be operated at a back pressure of 550 kPa (80 psi) to suppress cavitation.

VIEW A-A
SCALE 2/1

DIMENSIONS IN MM (IN.)

Fig. 1-16. Auxiliary water turbine drive on main circulator shaft

1.21.2. Steam Generator

The function of the steam generator is to transfer heat from the primary coolant (helium) to the secondary coolant (water/steam) during normal plant operation. The steam generator will be subject to cyclic or repeated steady and transient operating conditions in its 30 years of design life. The objective of this task is therefore to design and develop a steam generator which meets the operational, performance, and safety requirements of the GCFR.

The steam generator design effort during this quarter concentrated on a two-loop demonstration plant designed to be prototypical of the six-loop commercial plant and was sized based on the six-loop commercial plant conditions listed in Table 1-3. A decision was made to continue with the three-loop demonstration design and future efforts will be on this size range. The steam generator duty in the six-loop plant is slightly higher than that in the two-loop demonstration plant (Ref. 1-7).

Two basic constraints were applied when sizing the steam generator using the NUSIZE computer program (Ref. 1-8): (1) the maximum allowable height, which is provided by the preliminary PCVR layout, and (2) the maximum helium-side pressure drop. The sizing data in Table 1-4 consider these two constraints.

The helical bundle size can be made smaller by using smaller tubes. However, this would increase the water-steam-side pressure drop. A large water-/steam-side pressure drop tends to destabilize the boiling process, which limits the use of smaller tubes. In addition, a large number of tubes would be employed with a small tube o.d., increasing the manufacturing cost and making coil threading more difficult. Tubes with a small o.d. increase the number of tubes and the superheater tube sheet size, which worsens thermal stresses. A tube o.d. of 27 mm (1.063 in.) was selected as a compromise between the bundle size, stability, manufacturing cost, and tube sheet size. Parametric and cost effectiveness studies have not been performed.

TABLE 1-3
GCFR STEAM GENERATOR WITH 100% LOAD
PER LOOP

	2-Loop Demonstration Plant	6-Loop Commercial Plant
Duty [MW(t)]	571.5	614.0
Helium		
Flow rate [kg/s (lbm/s)]	450.43 (993.03)	456.40 (1006.19)
Temperature		
Inlet [°C (°F)]	537.8 (1000.0)	553.2 (1027.7)
Outlet [°C (°F)]	294.8 (562.6)	294.7 (562.4)
Pressure [MPa (psia)]	103.0 (1494.0)	103.1 (1494.6)
Total pressure drop [MPa (psi)]	0.42 (6.1)	0.43 (6.3)
Water/steam		
Flow rate [kg/s (lbm/s)]	219.74 (484.44)	240.78 (530.83)
Temperature		
Inlet [°C (°F)]	179.1 (354.3)	190.0 (374.0)
Outlet [°C (°F)]	510.0 (950.0)	510.0 (950.0)
Steam outlet pressure [MPa (psia)]	132.1 (1915.0)	132.1 (1915.0)

TABLE 1-4
SIZING DATA FOR HELICAL, TOP-SUPPORTED,
TOP EXHAUST GCFR STEAM GENERATOR

Installed surface area [m ² (ft ²)]	6,691 (72,025)
Bundle size	
Diameter (outside/inside) [mm (in.)]	4842/1422 (190.64/56.0)
Height [mm (ft.)]	7376.2 (24.2)
Number of tube columns	44
Tubes	
Material	2-1/4 Cr - 1 Mo
Outside diameter [mm (in.)]	27 (1.063)
Nominal wall thickness [mm (in.)]	478 (0.188)
Active length [m (ft.)]	119.5 (392)
Number	660
Pitch	
Transverse [mm (in.)]	38.86 (1.53)
Longitudinal [mm (in.)]	38.86 (1.53)
Support plates	
Full	7
Partial	7
Helium pressure drop [MPa (psi)]	0.39 (5.6)
Steam/water pressure drop [MPa (psi)]	34.48 (500)

The tube wall thickness is based on the minimum requirement of instantaneous buckling during one-loop dump. The helium in the steam generator cavity is assumed to be at maximum core pressure [12.5 MPa (1812 psia)] (Ref. 1-9) and a core outlet temperature which is [553.2°C (1027.7°F)]. Corrosion allowance, scratch allowance, and manufacturing tolerance were included in the nominal tube wall thickness [4.78 mm (0.188 in.)].

The number of support plates required is determined by the criterion of preventing excessive flow-induced vibration of helically coiled tubes (Ref. 1-10). The computer program WHIRL (Ref. 1-10) calculated critical velocity as a function of unsupported tube span, which determines the number of support plates necessary.

In this analysis, the support system consisted of seven full support plates and seven half support plates. The unsupported tube span and critical velocity varied from row to row.

With knowledge of the critical velocity at each row, the ratio of maximum to average velocity can be calculated (including a 20% safety margin):

$$V_{crit} = V_{ave} \times F_1 \times F_2 \quad ,$$

where V_{crit} = critical helium velocity,

V_{ave} = average helium velocity,

F_1 = safety margin (1.20 for each unsupported span),

F_2 = ratio of maximum to average velocity allowed for each unsupported span.

Thus,

$$F_2 = \frac{V_{crit}}{V_{ave} \times 1.20} ,$$

which is a function of radius, since the span lengths vary from row to row.

A flow distribution device must be installed above the bundle to maintain the velocity distribution specified by F_2 . The flow distribution is acceptable if the ratio of maximum to average velocity is less than F_2 at each row. The present conceptual design of the flow distribution device features a combination of a perforated cylinder and a perforated plate.

The following surface area allowances were provided to account for loss of active heat transfer area:

Tube plugging	1.1%
Shroud bypass flow effect	3.0%
Support plate blockage	4.2%
Total	8.3%

Fouling was included as fouling layer thermal conductance. Average fouling values during 5-yr cleaning cycles are 25,637 W/m²-°C (4,515 Btu/h-ft²-°F) in the evaporator and 76,93a7 W/m²-°C (13,550 Btu/h-ft²-°F) in the economizer.

Heat transfer and pressure drop uncertainties were estimated:

Gas-side heat transfer uncertainty	±10%
Steam-/water-side heat transfer uncertainty	±15%
Tube thermal conductivity and wall thickness uncertainty	±15%
Fouling uncertainty	±50%
Helium pressure drop uncertainty	±10%
Steam-/water-side pressure drop uncertainty	±10%

Figure 1-17 shows the steam generator arrangement. Because the design is top-supported, seismic restraint is required at the lower end of the bundle to restrict seismic vibration and impact load. A seismic restraint which is easily installed has been designed (Ref. 1-11).

The feedwater expansion loops are a critical area of concern for a top exhaust, top-supported steam generator. Hence, the thermally and seismically induced loadings on various steam generator configurations were investigated. Proper support of the expansion loop bundle enables a feasible steam generator design (Ref. 1-12).

1.23. HELIUM SERVICE SYSTEM

Evaluation of the impact of updated fuel performance data on plant design indicates that noble gas releases from the fuel to the helium purification system (HPS) will be approximately tenfold lower than previously estimated. (Shielding requirements, however, are reduced only about 12%.) This is a benefit for plant licensing considerations and eases the design requirements for the HPS.

Radiological dose calculations for depressurization accidents were made using the revised noble gas releases. These calculations indicate that with the containment isolated, the 10CFR100 dose limits for depressurization accidents can be satisfied without operation of the PES during the accident. Furthermore, improvements are possible if (1) the low population zone (LPZ) is increased to a more realistic distance or (2) the PES is assumed to operate during slow depressurization, i.e., with leaks up to about 6452 mm² (10 in.²).

1.28. CORE AUXILIARY COOLING SYSTEM (CACS)

1.28.1. Auxiliary Circulator, Valve and Service System

1.28.1.1. Auxiliary Circulator Design. The purpose of this subtask is to develop an auxiliary circulator which meets the performance, safety, and

Fig. 1-17. General arrangement of top-supported, top exhaust steam generator

reliability criteria for the CACS. The auxiliary circulator is designed to function as part of the core auxiliary cooling system (CACS) and circulate primary coolant for removal of residual heat from the core after the reactor has been shut down, including during refueling conditions.

Conceptual design work has been started on an auxiliary circulator installed from beneath the PCRV, i.e., a bottom-mounted circulator. Based on current estimated system parameters, either a centrifugal or axial compressor wheel could be adapted to satisfy the circulator operational requirements. Figure 1-18 shows a preliminary layout of a bottom-mounted axial flow auxiliary circulator; a similar layout with a centrifugal compressor arrangement is in progress. Figure 1-19 shows the motor assembly applicable to either an axial or centrifugal compressor. Both types of compressor wheels are being evaluated.

1.28.1.2. Auxiliary Loop Isolation Valve Design. The purpose of this subtask is to develop an auxiliary loop isolation valve which meets the performance, safety, and reliability criteria of the CACS. The auxiliary loop isolation valve is designed to function as part of the CACS to prevent backflow through an auxiliary loop when the main cooling loops are operating. The valve must open to permit core cooling by the CACS or during natural convection cooling.

The present reference design for the auxiliary loop isolation valve is a split butterfly configuration with two semielliptical flappers actuated by the differential pressure produced by gas flow. The design is similar to that used in the Fort St. Vrain HTGR except it is opened by gravity and compatible with natural convection cooling using the CACS. The valve is presently located in the vertical inlet duct of the auxiliary circulator as shown in Fig. 1-18. However, alternate valve locations and loop isolation methods which would eliminate or reduce the dependency on an isolation valve are being considered.

Fig. 1-18. Auxiliary circulator: axial flow, bottom mount

Fig. 1-19. Auxiliary circulator motor assembly: bottom mount

1.28.2. Core Auxiliary Heat Exchanger (CAHE)

The CAHE for an upflow core plant arrangement must perform under natural circulation conditions in the helium and water loops. A helical bundle was selected as the reference configuration, and a parametric study was performed utilizing the computer program described in Ref. 1-13 to determine the size of the helical bundle. Three design conditions were considered: design basis depressurization accident (DBDA) cooldown, natural convection cooldown, and pressurized forced-flow cooldown. A range of tube sizes varying from 20.3 mm (0.8 in.) to 63.5 mm (2.5 in.) was considered. This range is believed to be practical and satisfies the performance and structural design requirements. Figure 1-20 illustrates the range of acceptable bundle sizes, and Fig. 1-21 shows a general CAHE arrangement which incorporates a side penetration design for the water inlet and outlet pipes.

1.32. PLANT PROTECTION SYSTEM

The conceptual design of the plant protection system (PPS) continued. Plant layout criteria and technical descriptions have been developed, and work was initiated on a PPS development plan. Phase 1 residual heat removal (RHR) studies continued.

1.33. PLANT CONTROL SYSTEM

Work on a plant control system development plan was begun, and the differences between GCFR and HTGR analytical instrumentation are being evaluated.

1.35. DATA HANDLING SYSTEM

Operation of the prototype data acquisition, processing and display system was continued so that operational, reliability, and maintainability data can be obtained for use in the conceptual design of the system.

Fig. 1-20. Acceptable tube bundle sizes

Fig. 1-21. General arrangement of the CAHE helical bundle, side penetration

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2.0 REACTOR CORE

2.1. REACTOR CORE DEVELOPMENT

2.1.1. Materials Development

2.1.1.1. Fuel and Blanket Materials Technology. There was no activity on this subtask during this quarter.

2.1.1.2. Cladding, Shield, and Structural Materials Technology.

2.1.1.2.1. Cladding Technology

The GCFR cladding technology program is concerned with the study of mechanical, oxidation, and in-reactor creep behavior of GCFR ribbed and smooth (316 stainless steel) and advanced alloy cladding. The mechanical behavior test program on unirradiated ribbed and smooth cladding and the in-reactor creep test program are being conducted by Argonne National Laboratory (ANL). Pacific Northwest Laboratory (PNL) is conducting a mechanical test program in a recirculating helium loop, and the oxidation test program is being conducted at General Atomic (GA).

2.1.1.2.1.1. Cladding Oxidation Test Program. The overall objective of the GCFR cladding oxidation test program is to determine the oxidation behavior of ribbed and smooth 20% cold-worked 316 stainless steel and smooth candidate advanced alloy claddings. This includes determination of

1. Oxidation behavior of reference GCFR ribbed and smooth 316 stainless steel cladding fabricated by various techniques.
2. Oxidation behavior of smooth candidate advanced alloy claddings.

3. Effect of various pretreatments (i.e., cleaning procedures, passivation) on the oxidation behavior of ribbed and smooth cladding.
4. Oxidation behavior in the range of impurity levels expected in the GCFR demonstration plant.
5. External cladding corrosion allowance to be used in the fuel rod design.

The first oxidation test (OX-1) was completed. This test was conducted in a helium environment with an H_2/H_2O ratio of 10. The oxidation behavior of all the alloys tested was excellent. The second oxidation test (OX-2) was also completed. The test conditions were as follows:

Environment	Helium
Impurities	Hydrogen ($P_{H_2} = 100$ Pa)
	H_2O ($P_{H_2O} = 400$ Pa)
Temperature	600° and 700°C
Exposure	5000 h

Both iron and chromium oxides are stable in this environment, whereas only chromium oxides are stable in the OX-1 test environment. The alloys used in test OX-2 were

1. Austenitic steels (20% cold-worked 316 stainless steel and alloy 1.4981).
2. Ferritic alloys (HT-9 and D-57).
3. Precipitation-strengthened high-nickel alloys (M-813, PE-16, In-706, D-25, and D-68).

The stainless steel tubing was ribbed by three different fabrication techniques: electrochemical grinding, mechanical grinding, and electrochemical etching.

Figures 2-1 through 2-5 present the weight gain data from test OX-2. The following was observed:

1. There was unusual oxidation of ferritic alloys. Alloy D-57 had an excessive weight gain at 600°C and somewhat less of a weight gain at 700°C. At 250 h, blisters appeared on the surface of the oxide at 600°C. These blisters were analyzed by an energy dispersive x-ray technique and were determined to be pure iron oxide. The 700°C specimens did not have any blisters on the oxide surface, but the tubing appeared to be very brittle. These specimens were removed from the test prior to the goal exposure, because alloy D-57 was not tolerant of the environment of test OX-2.
2. Alloy HT-9 had better oxidation resistance than D-57 (Fig. 2-1). However, the spread of the weight gain data based on six specimens at 600°C was very large; i.e., the standard deviation was almost equal to the average weight gain. The 700°C test specimens were very brittle at the end of the test.
3. The oxidation resistance of 20% cold-worked 316 stainless steel appeared to be good, but oxide spallation was observed (both physically and by weight loss) on the etched and as-received test specimens at 700°C. This spallation caused the oxidation rate to be greater at 600° than at 700°C. Mechanically ground specimens of 316 stainless steel and alloy 1.4981 showed excellent oxidation resistance.
4. Precipitation-strengthened nickel alloys also had good to excellent oxidation resistance. However, these alloys are not of great interest for the GCFR.

Fig. 2-1. Weight gain of ferritic alloys

Fig. 2-2. Weight gain of nickel alloys

Fig. 2-3. Weight gain of stainless steel 316

Fig. 2-4. Weight gain of nickel alloys

Fig. 2-5. Weight gain of stainless steel 316

2.1.1.2.1.2. ANL Mechanical Testing Program. The objective of the ANL mechanical testing program is to determine the effects of the following factors on the behavior and mechanical properties of GCFR ribbed and smooth cladding:

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

Three biaxial creep rupture tests on unirradiated ribbed and smooth cladding have been completed, and the results indicate that the ribs increase rupture life and do not affect strain at failure. Test specimens for test ANL-IV and the first test at PNL (PNL-B10) were fabricated, and ANL has assembled a cladding specimen test rig in which the hoop and axial tensile stresses are equal. ANL has begun the fourth biaxial creep rupture test (ANL-IV) on ribbed and smooth GCFR cladding.

2.1.1.2.1.3. PNL Helium Loop Test Program. The primary objective of the PNL test program is to determine the effects of mass transfer on the creep rupture behavior of GCFR ribbed and smooth cladding. Biaxial creep rupture tests will be performed in the high-pressure helium loop, and the results will be compared with those of similar tests at ANL in a quasi-static helium environment. The first test was stopped at the end of ~100 h, but during this quarter, PNL successfully operated the empty loop with controlled impurity levels, and the first test (PNL-A10) was completed. Tables 2-1 and 2-2 give the test specifications and approximate test conditions. Table 2-3 presents creep rupture life and creep strain at failure. These results indicate that ribs strengthen the cladding. The creep strain at failure was not tabulated, but the average value was ~6%.

2.1.1.3. Trap Materials Technology. There is no work on this subtask this fiscal year.

TABLE 2-1
TEST SPECIFICATIONS FOR CREEP RUPTURE TEST PNL-A10

Loop pressure	20 atm
Loop temperature	650°C
Impurities	Hydrogen and water
Hydrogen partial pressure	300 Pa
Water partial pressure	30 Pa
H ₂ /H ₂ O	10
Specimen types	7 mechanically ground ribbed
	7 smooth
	6 electrochemically ground ribbed
Stress level	238 MPa (hoop)
	119 MPa (axial tensile)

TABLE 2-2
APPROXIMATE TEST ENVIRONMENT FOR
CREEP RUPTURE TEST PNL-A10

0 - 50 h	$H_2/H_2O < 2$ H_2 and H_2O levels unknown
50 - 100 h	$H_2/H_2O < 6$ H_2 and H_2O levels unknown
100 h to test completion	$P_{H_2} = 600 \text{ Pa}$ $P_{H_2O} = 60 \text{ Pa}$ $H_2/H_2O \sim 10$

TABLE 2-3
RESULTS OF CREEP RUPTURE TEST PNL-A10

Specimen	Hoop Stress Level (MPa)	Temperature (°C)	Rupture Life(h)	
			Average	Standard Deviation
Electrochemically ground ribbed	238	650	125.8	9.2
Smooth	238	650	116.4	10.4
Mechanically ground ribbed	238	650	136.3	18.9

2.1.1.4. Control Materials Technology. There is no work on this subtask this fiscal year.

2.1.1.5. Irradiation Testing.

2.1.1.5.1. F-1 Fast Flux Irradiation Experiment

Development of a method for determination of iodine in the F-1 charcoal trap samples continued. In the past, standard methods have been subject to large errors due to volatilization of iodine during sample dissolution. Analysis of the charcoal for iodine is necessary to determine the degree of transport of fission product iodine to the charcoal trap in the F-1 irradiation experiment. The fraction of iodine inventory transported to the charcoal trap in the GB-9 and GB-10 experiments is $\sim 10^{-4}$ (Refs. 2-1, 2-2).

Determination of iodine in the F-1 charcoal material is complicated for two reasons: the charcoal is very radioactive and is very difficult to dissolve, apparently because of densification of the charcoal (40% to 60% volume shrinkage) during fast flux irradiation. In contrast, the charcoal in the GB-9 and GB-10 experiments did not appreciably shrink under the thermal neutron irradiation conditions of those experiments.

The method chosen for determination of iodine in the charcoal samples is to dissolve the samples and determine the iodine concentration by activation analysis using the nuclides I-127 and I-129. Two approaches for dissolution of the F-1 charcoal were tried and abandoned. In the first approach (wet ash), dissolution of F-1 charcoal was attempted in hot (fuming) HClO_4 (perchloric acid). However, even after several days, the sample did not dissolve. The difficulty of dissolution may have been caused by the many layers of fission products. After attempts at leaching with hot acid mixtures, the wet ash (HClO_4) approach was abandoned.

The second approach (fusion technique) used inorganic salt fluxes. However, the charcoal floated on the surface of each melt, and hence, this approach was also abandoned.

A third approach (dry ash) will be tried. The charcoal samples will be burned in a tube furnace and the effluent gases will be vented to a sulfite trap solution with a stream of oxygen to ensure quantitative recovery of the iodine.

2.1.1.5.2. GB-10 Vented Fuel Rod Experiment

There was no activity on this subtask during this quarter.

2.1.1.5.3. F-5 (X317) Fast Flux Irradiation Experiment

The objectives of the F-5 experiment are as follows:

1. To irradiate GCFR ribbed fuel rods to high burnups and fluences so that fuel rod performance under expected GCFR linear power and temperature conditions can be studied.
2. To study the effect of a step change in power increase that simulates the 180-deg rotation of a GCFR fuel assembly at the core-blanket interface.
3. To determine the performance of a prototypical grid-spaced bundle structure.
4. To investigate the performance of an alternative design for the fuel - axial blanket interface to accommodate cesium migration.
5. To determine probable modes of cladding breach at high burnup.

Irradiation of the F-5 grid-spaced bundle in EBR-II began in run 93. The first phase of the irradiation was completed in mid-run 100. The calculated burnup during this phase was ~ 4.34 at. % with a corresponding fast fluence of $\sim 5 \times 10^{26}$ n/m² ($E > 0.11$ MeV). The second phase of the irradiation will use 14 preirradiated rods and 5 new rods in a 19-rod grid-spaced subassembly. This assembly will be irradiated in a row 7 position (at about 70% of the original power rating) in EBR-II for 2 cycles. At the end of this phase, the fuel rods will be reconstituted into a 31-rod subassembly for operation at full power. This sequence of operation will determine the effect of a step change in power on the performance of the fuel rods.

Prior to the low-power operational phase, the irradiated fuel rods will be depressurized and resealed such that cladding creep due to fission gas pressure buildup is reduced, thus simulating the GCFR. Depressurization and resealing involves the following steps:

1. The end caps near the plug are cut off.
2. The plug is machined to fit the new end caps.
3. The plug is drilled, mainly by conventional means.
4. The plug is punched by laser drilling, and the fission gases are collected.
5. The fuel rod is filled with new tag gas.
6. The new end caps are welded.

Depressurization and resealing of the fuel rods are expected to be done in the near future, and the reconstituted 19-rod subassembly is expected to be inserted in the core prior to run 107.

The 19-rod subassembly thermal-hydraulic analysis has been completed, and the data have been sent to EBR-II for flow testing.

As described in Ref. 2-3, eight F-5 fuel rods were gamma-scanned at the hot fuel examination facility (HFEF) when the 31-rod subassembly (X317) was removed at the end of the first phase of the F-5 irradiation experiment. There are ten scans for each fuel rod: (1) top of assembly; (2) plenum; (3) full rod length (except rod top and plenum); (4) top of fuel region; (5) bottom of fuel region; (6) fission product trap; and (7-10) diametral scans for regions above and below the upper and lower fuel-blanket interfaces.

A computer routine has been set up to process the F-5 gamma spectra for the nuclides in Table 2-4 as well as similar spectra for other irradiation experiments. Figures 2-6 through 2-9 show four representative plots for scan 3 (full axial rod length) of rods GH-77 and GE-64. The Zr-95 plot (Fig. 2-6) delineates the blanket and fuel regions and represents the fission density profile in the fuel rod. Figure 2-7 shows the axial distribution of I-131 and indicates that there is an extremely small fraction of I-131 in the trap region, which is located between about 889 and 991 mm (35 and 39 in.) on the distance axis. Figures 2-8 and 2-9 indicate that there are appreciable fractions of Cs-137 in the trap region. The Cs-137 appears to be concentrated at the inlet interface of the trap material. The Cs-137 peak in the trap region occurs at 914 mm (36.0 in.) on the distance scale for rod GH-77 (which contained graphite trap material) and at 955 mm (37.6 in.) for rod GE-64 (which contained charcoal trap material). This difference in peak location was caused by greater shrinkage of the charcoal during irradiation and the axial relocation of the interface due to this shrinkage. As indicated in Ref. 2-3, the charcoal and graphite shrinkages were about 55% and 10%, respectively.

Table 2-5 presents the distribution data for Zr-95, I-131, and Cs-137 obtained from integrating scan 3 values over axial distance. The values for the three isotopes in the blanket and fuel regions are considered to be

TABLE 2-4
NUCLIDES USED IN PROCESSING OF F-5 GAMMA SPECTRA

<u>No.</u>	<u>Nuclide and Energy (KeV)</u>
1	Ce-144 (80.1), Xe-133 (81.0)
2	Ce-144 (133.5)
3	Ce-141 (145.4)
4	Te-132 (228.3)
5	I-131 (364.5)
6	Sb-125 (427.5)
7	Ba-140, La-140 (487.0)
8	Ru-103 (497.1)
9	Nd-147 (531.0)
10	Ru-106 (621.9)
11	Cs-137 (661.6)
12	Zr-95, Nb-95 (765.8)
13	Cs-134 (795.5)
14	Co-58 (810.8)
15	Mn-54 (834.8)
16	Cs-136 (1048.1)

Fig. 2-6. Distribution of Zr-95 in rod GH-77 irradiated in F-5 experiment

Fig. 2-7. Distribution of I-131 in rod GH-77 irradiated in F-5 experiment

Fig. 2 8. Distribution of Cs-137 in rod GH-77 irradiated in F-5 experiment

Fig. 2-9. Distribution of Cs-137 in rod GE-64 irradiated in F-5 experiment

TABLE 2-5
F-5 FUEL ROD FISSION PRODUCT DISTRIBUTION

Rod	Blanket	Trap Material	Fission Product	Fission Product Distribution (%) ^(a)			
				Upper Blanket	Fuel	Lower Blanket	Trap
GA-31	Nominal	Charcoal	Zr-95	1.6	97.4	1.0	Low (<0.01)
			I-131	2.4	96.4	1.1	Low (<0.1)
			Cs-137	6.3	85.9	2.0	5.7
GA-37	Nominal	Charcoal	Zr-95	1.5	97.6	0.9	Low (<0.01)
			I-131	1.9	97.1	0.9	Low (<0.1)
			Cs-137	6.7	91.1	1.6	0.5 ^(b)
GC-59	Nominal	Graphite	Zr-95	1.5	97.5	1.0	Low (<0.01)
			I-131	1.5	97.1	1.1	Low (<0.1)
			Cs-137	6.1	86.7	1.6	5.6
GD-60	Nominal	Graphite	Zr-95	1.5	97.5	1.0	Low (<0.01)
			I-131	1.8	97.1	1.0	Low (<0.1)
			Cs-137	7.3	88.1	1.7	2.9
GE-64	Modified	Charcoal	Zr-95	1.6	97.4	1.0	Low (<0.01)
			I-131	1.9	97.1	1.0	Low (<0.1)
			Cs-137	6.2	87.9	2.5	3.4
GE-65	Modified	Charcoal	Zr-95	1.6	97.5	0.9	Low (<0.01)
			I-131	1.8	97.2	0.9	Low (<0.1)
			Cs-137	5.2	88.1	3.7	3.0
GG-75	Modified	Graphite	Zr-95	1.5	97.5	1.0	Low (<0.01)
			I-131	2.0	97.0	0.9	Low (<0.1)
			Cs-137	9.9	82.9	2.6	4.5
GH-77	Modified	Graphite	Zr-95	1.5	97.6	0.9	Low (<0.01)
			I-131	1.6	97.2	1.0	Low (<0.1)
			Cs-137	3.8	90.6	2.1	3.6

(a) These values were obtained from scan 3 data. The trap values are relatively uncertain because (1) the CPM values were relatively low for Zr-95 and I-131 in the trap region, and (2) the Cs-137 was concentrated within a short distance in the trap region.

(b) This value is too low.

fairly accurate since the net CPM values in these regions were substantially above background level. The distribution values for Zr-95 and I-131 in the trap region were too low to be determined with any degree of accuracy. The CPM values for the two isotopes in the trap region occur at random locations, are essentially at background levels, have high sigma (error) values, and are thus uncertain and questionable. Nevertheless, integrations were performed, and the results were used to obtain the upper-limit distribution values of 0.01% for Zr-95 and 0.1% for I-131 in Table 2-5. The distribution values for Cs-137 in the trap region are relatively high (compared with the values for the other two isotopes), but are also fairly uncertain for the following reason. The CPM values for Cs-137 at each location in the trap region were low except for a sharp peak at the trap material (graphite or charcoal) interface. All or part of this peak could have been missed in the full-length gamma scan. Closely spaced scans of the trap material interface and regions bounding the fuel-blanket interface are sometimes required to obtain good data on Cs-137.

The distribution of Zr-95 given in Table 2-5 and Fig. 2-6 represents the fission density distribution. Zirconium in an oxide matrix is a refractory element and does not perceptively migrate under the irradiation conditions of this experiment. The distributions of Zr-95 and I-131 were similar, as shown by Table 2-5 and Figs. 2-6 and 2-7. This is remarkable considering the big difference in the chemical characteristics and boiling of zirconium and iodine and shows that the transport of I-131 is small. This finding is consistent with the small transport of I-131 observed in the GB-9 and GB-10 experiments (Refs. 2-1, 2-2). The small transport of I-131 is attributed to its relatively short half-life (8 days) and to the high chemical affinity of iodine for electropositive fission product elements, such as barium and strontium, that stay with the fuel. This may conflict with the fact that iodine combines with cesium, which migrates appreciably.

Comparison of the distribution data for Zr-95 and Cs-137 in Table 2-5 and Figs 2-6, 2-8, and 2-9 shows that Cs-137 migrated appreciably into the

blanket and trap regions. It is surmised that this migration resulted principally from metal vapor transport or surface diffusion. It is unlikely that the amount of Cs-137 transported could have resulted from the relatively short-lived (3.9 min) xenon precursor.

The Cs-137 distribution value of 0.5% given in Table 2-5 for the trap region of rod GA-37 is inordinately low. To determine if this value is real or in error, the Cs-137 CPM data for scan 6 (fission product trap region) were integrated for the eight rods. The resulting integration values are as follows:

Rod	Cs-137 Integration Value (CPM x Distance) for Scan 6 (Trap Region)
GA 31	1.19×10^4
GA-37	8.85×10^3
GC-59	2.10×10^4
GD-60	2.01×10^4
GE-64	1.03×10^4
GE-65	8.50×10^3
GG-75	1.70×10^4
GH-77	1.87×10^4

Comparison of these values indicates that the fractions of Cs-137 in each of the rod trap regions are consistent within a factor of about two, leading to the conclusion that the value of 0.5% in Table 2-5 is wrong (too low). Apparently, the Cs-137 peak was missed in scan 3 of rod GA-37. The increment for scan 3 was 5.08 mm (0.2 in.) and that for scan 6 was 2.54 mm (0.1 in.).

Examination of the data in Table 2-5 indicates that design modifications incorporated into some of the F-5 rods had no effect on fission product transport behavior. These design modifications consisted of (1) use of low-density blanket pellets at the fuel-blanket interface to accommodate cesium migration and (2) use of graphite in place of charcoal in some rods.

Reference 2-3 (Table 2-1) gives preliminary F-5 fission product (Cs-137 and I-131) distribution data for rods GH-77 and GE-64. These are in reasonable agreement with the data in Table 2-5, except for the earlier Cs-137 trap value of 0.6% for rod GE-64. This value is considered to be in error (too low).

2.1.2. Assembly Design Verification and Support

2.1.2.1. Out-of-Pile Tests.

2.1.2.1.1. Assembly Thermal-Hydraulic Tests

The effectiveness of flow mixing vanes in reducing diametral fuel rod gradients in a GCFR fuel assembly continues to be studied.

2.1.2.1.2. Assembly Mechanical Tests

There was no activity on this subtask during this quarter.

2.1.2.1.3. Assembly Vibration and Seismic Tests

A major concern in the design of GCFR core assemblies is the amount of flow-induced vibrations present in the individual fuel rods, the fuel rod bundle, and the whole assembly. Flow-induced vibration levels have been predicted by analysis and observed in GCFR experiments. Although the evaluations and experience to data have generally been favorable, specific tests are required to assess actual flow-induced vibration levels.

2.1.2.1.3.1. Helium Flow Test Rig. The helium flow test rig is a high-pressure helium flow circuit capable of testing GCFR fuel bundles of up to 37 rods at full flow. Construction of the rig was completed this quarter (Fig. 2-10), and the rig is being checked out.

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Fig. 2-10. Helium flow test rig

2.1.2.1.3.2. Single-Rod Test Section. A special process has been developed for fabricating simulated fuel pellets. The pellets are cast from a lead-antimony alloy using a modified bullet mold and are then passed through a sizing die to achieve a diametral tolerance of ± 0.01 mm. Approximately 500 pellets have been manufactured. Final assembly of the single-rod test section is under way.

2.1.2.1.3.3. Single-Rod Test Programs. The objectives of the single-rod helium flow vibration tests are to evaluate the flow-induced vibrations of individual GCFR fuel rods and to establish the effects of cladding ribbing, rod-to-grid clearance, and grid-to-grid spacing on vibration levels. Smooth and ribbed rods with rod-to-grid clearances of 0.1, 0.2, and 0.3 mm and grid-to-grid spans of 200, 300, and 400 mm will be evaluated.

Table 2-6 gives the complete matrix for possible single-rod tests. The total number of possible combinations of ribbing, clearance, and span is 18, with each combination constituting a separate test. Each test requires about one week, and additional time is needed between certain groups of tests. Eighteen tests would consume a considerable amount of time. Fortunately, it has been possible to eliminate some of the 18 tests and still fulfill the test objectives. It is not necessary to duplicate every ribbed rod test with a smooth rod test. Since the rod-to-grid clearances of most interest are 0.1 and 0.2 mm and the grid-to-grid spans of most interest are 200 and 300 mm, the ST400-X series of tests and the STXXX-3 tests can be eliminated, reducing the total number of tests from 18 to 13. Four smooth rod tests will remain for comparison with equivalent ribbed rod cases. The RT 400-X tests are of least interest, and hence it is proposed that these be given second priority. They can be completed as time permits, after the other tests have been completed. This would reduce the total number of tests to ten, which should be sufficient to fulfill the test objectives. These 10 tests will be known as the phase 1 single-rod tests, and a tentative timetable for these tests is given in Table 2-7. The total duration of the tests is 18 weeks.

TABLE 2-6
MATRIX OF POSSIBLE SINGLE-ROD TESTS(a)

Rod-to- Spacer-gap,	Grid-to-Grid Pitch		
	200 mm	300 mm	400 mm
0.1	ST200-1(a) RT200-1	ST300-1 RT300-1	ST400-1 RT400-1
0.2	ST200-2 RT200-2	ST300-2 RT300-2	ST400-2 RT400-2
0.3	ST200-3 RT200-3	ST300-3 RT300-3	ST400-3 RT400-3

(a)S = smooth cladding; R = ribbed cladding; ST200-1 = smooth cladding, 200-mm grid pitch, 0.1-mm rod-to-grid gap.

TABLE 2-7
PHASE 1 SINGLE-ROD TESTS

Test Series	Specific Run	Duration (weeks)
	Priority tests	
A	ST200-1	3
	ST200-2	
	Bundle rebuild 1	1
B	ST300-1	3
	ST300-2	
	Bundle rebuild 2	2
C	RT300-1	4
	RT300-2	
	RT300-3	
	Bundle rebuild 3	1
D	RT200-1	
	RT200-2	
	RT200-3	
	Optional tests	
	ST200-3	
	ST300-3	
	ST400-1	
	ST400-2	
	ST400-3	
	RT400-1	
	RT400-2	
	RT400-3	

2.1.2.1.4. Component Flow Tests

There was no activity on this subtask during this quarter.

2.1.2.1.5. Core Flow Test Loop

A series of out-of-pile simulation tests will be conducted to demonstrate the ability of the GCFR fuel, control, and blanket assembly designs to meet design goals and to verify predictions of analytical models which describe operation and accident behavior. The tests will measure 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 core flow test loop (CFTL) program plan (Ref. 2-4) describes the requirements for the test program to be conducted in the CFTL, which will be constructed and operated by Oak Ridge National Laboratory (ORNL).

The stage I CFTL configuration, to be completed by the fall of 1981, will be capable of performing all 37-rod bundle, AG-1 tests to simulate normal, upset, emergency, and faulted GCFR conditions except rapid depressurization. Figure 2-11 shows the loop configuration, and Fig. 2-12, an elevation view for the first test.

Thermal-flow-structural analysis of the test models and the loop has been performed in support of the AG-1 design. The TSPEC code (Ref. 2-5) is being modified so that it is in agreement with current methods.

2.1.2.1.5.1. CFTL/AG-1 Thermal-Hydraulic Analysis. Previous thermal-hydraulic design analysis of the AG-1 37-rod bundle (Ref. 2-6) calculated two accident conditions at quasi-steady-state conditions: (1) loss of forced circulation (LOFC) at full nominal pressure, which is beyond the design basis (3.75% flow, 2% power) and (2) DBDA (2% flow, 4% power). The analysis showed that without test section heat loss, maximum rod diametral

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Fig. 2-11. CFTL configuration

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Fig. 2-12. Elevation view of CFTL for first test

temperature differences are about 5°C and rod surface temperatures do not exhibit swings. Test section heat loss was calculated to be about 5% (1.14 kW) of the bundle input power for the pressurized case and 13% (5.35 kW) for the depressurized case. Incorporation of test section heat losses for two design basis depressurization accident (DBDA) cases resulted in substantial surface temperature swings (as much as 64°C for the depressurized case) but low (10°C maximum) edge rod diametral temperature differences.

Polishing of the outside surface of the test duct and the inside surface of the attemperation flow annulus reduced the unpressurized DBDA condition test section heat loss from 13% to 4% of the bundle input power.

2.1.2.1.5.2. AG-1 Design and Fabrication. Acceptance of the cladding tubing to be used for fuel rod simulators was delayed by inconclusive results of ultrasonic examination, which indicated tubing flaws. Additional tests are being conducted.

2.1.2.2. Pressure Equalization System. There was no progress on this sub task during this quarter.

2.1.2.3. Assembly Methods Development.

2.1.2.3.1. PES Methods Development

There was no progress on this subtask during this quarter.

2.1.2.3.2. Design Basis Transient Testing

2.1.2.3.2.1. Fuel Cladding Faulted Temperature Limit. A fuel and blanket cladding faulted temperature limit of 1300°C has been selected (Ref. 2-6), and the residual strength of stainless steel needed at this limit was examined. The phenomena of deadweight, seismic forces, and rod bowing/relaxation were considered along with the fuel and cladding conditions which

determine deadweight stress. Deadweight stress on the cladding is estimated to be only 3 to 5 MPa. The seismically induced stresses in the cladding can only be estimated, but the acceptance criterion has been established. The transient temperature ramp of the DBDA will be compared with the likely duration of the safe shutdown earthquake (SSE), and the core will be designed for a conservative combination of cladding temperature and seismic force. Rod bowing and stress relaxation during high-temperature transients were examined to ensure that bowing would not increase during the transient.

The effects of rod-spacer interaction on the faulted temperature limit were also examined. During the heat-up period of a DBDA, the temperatures of most components at a given axial location within the core assemblies do not vary widely. The average cladding temperature at a given axial location will, however, always be slightly hotter than that of the assembly structural components (spacer grids, grid supports, and flow duct). Hence, there is a potential for differential thermal expansions.

The most significant differential expansions occur in the axial direction and are normally accommodated by rod motion through the grids (slippage between the rods and grids). At the very high temperatures which occur during a DBDA, there could be a tendency for the rods to self-weld or otherwise seize in the grids. Should this happen, differential expansion would have to be accommodated by component deformation. Although deformation may be acceptable, one verification test is designed to demonstrate that fuel rods can slide through the grid spacers.

Fuel-cladding mechanical interaction (FCMI) appears to have no affect on selection of temperature limit. The GCFR fuel rod is designed to have an initial nominal pellet-to-cladding gap of 0.14 mm. When the reactor is brought up to power, the gap is reduced to less than 50 μ m at the peak power position owing to differential thermal expansion. During reactor operation, the gap tends to close more because of fission product production. Analyses using the LIFE-III code (Ref. 2-7) indicate that the pellet-to-cladding gap

is never completely closed during steady-state lifetime operation. After an initial incubation period corresponding to a burnup of approximately 4 at. % and a fluence of approximately 5×10^{22} n/cm², the pellet-to-cladding gap begins to open owing to fluence-induced metal swelling of the cladding. As a consequence, there is very little FCMI in the GCFR rod design.

The most limiting faulted events (DBDA and LOFC) are undercooling events under reactor shutdown conditions. During these events, average fuel temperature decreases and cladding temperature increases. When the cladding temperature reaches a maximum during a DBDA, there is less than a 50°C difference between the cladding midwall temperature and the pellet center-line temperature. Since the average pellet temperature is reduced and the cladding temperature increases, the pellet-to-cladding gap increases during the limit faulted events. Consequently, FCMI is not a limiting parameter in the selection of the cladding faulted temperature limit. A test program with irradiated rods is planned to verify this conclusion.

Fuel cladding chemical interaction (FCCI) has been observed in most LMFBR rods irradiated at temperatures >600°C. FCCI was also observed in GCFR irradiation experiment F-1 (X094). The extent of cladding attack in the F-1 experiment was well within the limits of the attack expected based on the large LMFBR data base (Ref. 2-8). Since the GCFR cladding has no primary stress loadings at any time in its life, the effect of FCCI on strength and faulted temperature limit is expected to be negligible.

2.1.2.3.2.2. Verification Test Programs. Figure 2-13 shows the information to be obtained in the verification test programs. The rod-spacer interaction test is designed to obtain separate effects data for only rod-spacer interactions, but it also provides information on deadweight and seismic and rod bowing effects. The CFTL simulates the bundle effects of deadweight, rod bowing, and rod-spacer interaction and is therefore classified partially representative of bundle performance. Only the in-pile

Fig. 2-13. Verification test programs

tests are classified as fully representative of bundle performance for phenomena such as FCMI and irradiation effects. When these tests are completed, they are expected to verify the acceptability of the 1300°C cladding temperature limit for faulted condition transients.

2.1.2.3.3. Thermal-Hydraulic Methods Development

2.1.2.3.3.1. COBRA*GCFR Modifications. COBRA*GCFR (Refs. 2-9, 2-10) is a modified version of the COBRA-IV code (Ref. 2-11). Modifications have been incorporated into the COBRA*GCFR code so that laminar flow analysis can be done correctly.

The entrance effect on the laminar heat transfer coefficient is described by (Refs. 2-12, 2-13):

$$St_x = St_\infty \left(1 + \frac{1.99}{Z^{0.506} e^{0.041*Z}} \right), \quad (2-1)$$

where St_x = Stanton number at location x ,

St_∞ = Stanton number for fully developed conditions ,

$$Z = \left(\frac{1}{Gz} \right) * 10^3 ,$$

Gz = Graetz number = $RePr/(x/de)$,

x = distance from beginning of heating.

Equation 2-1 has been programmed into the COBRA*GCFR code and the entrance effect applied to the heat transfer coefficient whenever the Reynolds number is less than 3000.

*A function of the confidence level for maximum depth of interaction. For $K = 0$, the equation gives the median value of the attack; for $K = 12.23$, the equation gives the maximum attack at the 95% confidence level.

The procedure to calculate the circumferential rod conduction described in Ref. 2-10 has been modified as follows. The momentum, energy, and continuity equations are solved to calculate the coolant temperatures and heat transfer coefficients at a given axial node. This information is used in the subroutine CIRCON (Ref. 2-10) to calculate the local heat flux for each rod and channel. In CIRCON, the radiation heat transfer affects the boundary conditions at the surface as follows: at $r = R_0$,

$$-K \frac{\partial T}{\partial r} = h(T_S - T_B) + q_R'' \quad , \quad (2-2)$$

where K = thermal conductivity of the cladding,
 T = temperature at location r ,
 r = distance from the center of the rod,
 h = heat transfer coefficient,
 T_S = surface temperature,
 T_B = coolant bulk temperature,
 q_R'' = net radiation heat flux leaving the surface,
 R_0 = outside radius of the surface.

The conservation equations are resolved with the updated heat fluxes, and calculations proceed to the next axial level after convergence is reached at the current axial level. The new conduction procedure requires slightly larger computer core storage, but it is considerably more convergent.

Reference 2-14 explains the procedure for calculating the radiation exchange between grey surfaces. The net radiation exchange in an enclosure occurs by a complex process involving multiple reflections from all surfaces. The Hottel total radiation exchange factor (Ref. 2-14) F_{I-J} is defined so that

$$Q_{R_{I \rightarrow J}} = A_I F_{IJ} \sigma (T_I^4 - T_J^4) \quad , \quad (2-3)$$

where $Q_{R \rightarrow I+J}$ = net radiation exchange between surfaces I and J,
 A_J = area of surface J,
 F_{IJ} = Hottel total radiation exchange factor between surfaces I and J,
 σ = Stefan-Boltzman constant,
 T_J = surface temperature of surface J in degrees Kelvin.

The total radiation exchange factors $\bar{F}_{IJ}$ is evaluated by solution of

$$\begin{array}{rcl}
 \left(\bar{11} - \frac{A_1}{\rho_1} \right) \cdot {}_1R_1 + \bar{12} \cdot {}_1R_2 & + \bar{13} \cdot {}_1R_3 + \dots + \bar{1R} \cdot {}_1R_R & + \bar{1S} \cdot {}_1R_S + \dots = -\bar{11} \cdot \epsilon_1 \\
 \bar{12} \cdot {}_1R_1 & + \left(\bar{22} - \frac{A_2}{\rho_2} \right) {}_1R_2 + \bar{23} \cdot {}_1R_3 + \dots & + \bar{2R} \cdot {}_1R_R + \bar{2S} \cdot {}_1R_S + \dots = -\bar{12} \cdot \epsilon_1 \\
 \vdots & & \vdots \\
 \bar{1R} \cdot {}_1R_1 & + \bar{2R} \cdot {}_1R_2 & + \bar{3R} \cdot {}_1R_3 + \dots + (RR - AR) {}_1R_R + \bar{RS} \cdot {}_1R_S & \neq \dots = -\bar{1R} \cdot \epsilon_1 \\
 \bar{1S} \cdot {}_1R_1 & + \bar{2S} \cdot {}_1R_2 & + \bar{3S} \cdot {}_1R_3 + \dots + (RS \cdot {}_1R_R + (\bar{SS} - A_S) {}_1R_S & + \dots = -\bar{1S} \cdot \epsilon_1 \\
 \vdots & & \vdots & \vdots \\
 \vdots & & \vdots & \vdots
 \end{array} \quad (2-4)$$

where $\bar{IJ} = A_I F_{IJ} = A_J F_{JI}$,
 $\bar{F}_{IJ}$ = view factor ,
 ${}_1R_J$ = radiant flux per unit area at surface J when $\sigma T_I^4 \equiv 1$,
 ϵ_I = emissivity of surface I ,
 $\rho_I = 1 - \epsilon_I$

with

$$A_I F_{In} = {}_1R_n \cdot A_n \frac{\epsilon_n}{1 - \epsilon_n} \quad (2-5)$$

For evaluation of F_{2n} , the right-hand side of Eq. 2-4 is replaced by $-2N\epsilon_2$ ($N = 1, \dots$); R and S are the reradiating surfaces. The total radiation exchange factors F_{IJ} satisfy the relation

$$\sum_{J=1}^N F_{IJ} \equiv \epsilon_I \quad (2-6)$$

The program HOTTEL (Ref. 2-15) calculates the total radiation exchange factors F_{IJ} and verifies Eq. 2-6. In the COBRA*GCFR code, all F_{IJ} factors which are less than 2% of ϵ_I are ignored to reduce the storage requirement (for benchmark V tests 2 and 3 this reduces the number of F_{IJ} quantities from $250 * 250 = 62,500$ to 2,500). For computers without storage limitations, this condition can be removed.

The net radiant heat flux from a surface I is calculated by the relation

$$q_R(I) = \sum_{J=1}^N \sigma F_{IJ} (T_I^4 - T_J^4) \quad (2-7)$$

Figure 2-14 shows the scheme of including radiation heat transfer in thermal-hydraulic calculations.

With heat loss from the assembly, the following equation relates the duct temperature to the coolant bulk temperature in the wall channel:

$$h_D(T_B - T_D) = Q_L/A, \quad (2-8)$$

where h_D = heat transfer coefficient between the duct surface and the coolant,

T_B = coolant bulk temperature in the wall channel,

T_D = duct temperature,

Q_L = net heat loss from the channel,

A = heat transfer area for the heat loss.

Fig. 2-14. Flow diagram for circumferential conduction and radiation in COBRA*GCFR code

The duct temperature T_D is calculated in the COBRA*GCFR code when heat loss is modeled.

2.1.2.3.3.2. Thermal-Hydraulic Codes. The three tests selected from AGATHE-1C tests for benchmark V calculations were

1. Laminar flow without power tilt.
2. Laminar flow with power tilt.
3. Turbulent flow with power tilt.

Figure 2-15 and Table 2-8 compare the measured values and analytical predictions of the CLUHET (Ref. 2-16), COBRA*GCFR (Ref. 2-9), SAGAPU (Ref. 2-17), SCANDAL (Ref. 2-18), and SCRIMP (Ref. 2-19) codes. For the COBRA*GCFR code, the predicted pressure drop agrees closely with of the measured pressure drop. The temperatures predicted by all codes at level 5 are within 10%. All codes calculate higher temperatures than the measured temperatures. This may be due to underprediction of heat loss from the apparatus at the hotter exit end.

Analytical predictions for the bundle pressure drop were within 10% of the measured pressure drop. However, the surface temperature predictions were off by up to 100°C. This was attributed to (1) incomplete description of the heat loss model and (2) measurement uncertainty with laminar flow and power tilt. For turbulent flow with power tilt, the agreement between predictions and measurements was within $\pm 5\%$.

The following has been concluded:

1. The COBRA*GCFR code adequately performs the thermal-hydraulic calculations for all flow regimens.
2. Radiation and conduction heat transfer modes are important during laminar flow calculations.

Fig. 2-15. Bundle pressure drop test 1

TABLE 2-8
COMPARISON OF CALCULATED AND MEASURED TEMPERATURES AT
RADIAL THERMOCOUPLE POSITIONS

Rod	Subchannel	Temperature(°C)					
		SCRIMP	CLUHET	COBRA* GCFR	SCANDAL	SAGAPO	AGATHE(a) Hex 1
1	1	695.1	691.1	710.6	707.4	717.8	689.8
2	3	692.0	690.4	709.7	702.6	713.9	601.0 601.5
4	6	681.0	686.0	706.7	686.6	693.9	671.2 671.3
6	4	682.2	686.7	705.0	686.1	692.1	669.7
6	9	679.3	686.4	696.5	686.9	692.3	666.7
7	10	664.5	670.9	674.9	660.2	668.1	633.0
7	11A	665.3	671.2	674.9	659.7	667.7	635.2
8	6	676.3	677.2	685.7	666.9	678.2	644.7 649.0
8	7	675.9	677.1	683.0	667.3	678.2	644.3
8	11C	674.3	676.3	682.3	668.0	678.0	641.5
8	12A	673.0	676.4	677.7	668.3	678.1	640.3
8	12B	674.7	676.4	679.9	667.9	678.1	—
9	12C	674.7	675.9	682.2	667.8	677.7	649.7
9	12D	673.0	675.9	680.4	668.2	672.7	642.4
10	9	668.6	672.2	691.5	658.6	665.9	637.9
10	13	666.0	671.5	670.3	659.4	665.6	633.5 633.7
10	14	664.4	671.2	670.3	660.0	666.1	631.8
Shroud	11/12	582.6	582.1	563.9	585.5	577.1	525.9 524.7
Shroud	13/14	582.5	582.1	568.0	585.9	569.2	524.4 522.5 523.4

(a) Measured.

3. Accurate description of heat loss models is important for laminar flow analysis.

The radial blanket assemblies of a fast breeder reactor produce less power than the fuel assemblies. As a result, under normal operating conditions it is desirable to channel the majority of the coolant through the active core. When reactor power is reduced, the ratio of blanket to core power increases. This, in the event of a protected loss of coolant flow event, the cooling requirements of the blanket assemblies become relatively more severe. It is therefore important to identify any flow redistribution that may occur between the core and the blanket under conditions of low power and flow.

An effort is under way to develop methods for calculating the total flow distribution and resulting cladding temperatures under conditions of pressurized and depressurized protected loss of flow. The primary tool will be GACCOOL (Ref. 2-20), a thermal-hydraulic analysis code. The code has been revised so that all quantities are in metric units, and it may be easily redimensioned for various problem sizes by alteration of a single parameter statement. The existent turbulent heat transfer and friction factor correlations for fuel assemblies have been updated to reflect current best estimate formulations, and laminar correlations were added, permitting reduced flow calculations. Subassembly calculations are being carried out using GCFR*GOBRA to verify GACCOOL results and identify significant discrepancies between the simple, single-channel GACCOOL results and a complete assembly analysis. Where discrepancies exist, correction factors are being developed for inclusion in GACCOOL. Similar modifications will be performed for the blanket, control, and shutdown assemblies.

2.1.3. Core Design Verification and Support

2.1.3.1. Test Programs.

2.1.3.1.1. Critical Assembly Experiments

Analysis of the zone steam flooding experiment in the phase 3 GCFR critical assembly has begun. In these experiments, entrance of about 45 g/l steam in the coolant channels was simulated with polyethylene (CH₂) foam inserted into the void channels of a central 5 x 5 drawer region of core zone 1 and its axial blankets. The worth of the CH₂ "flooding" was measured in plate and rod type loadings of the central region. The analyses for this experiment require 28-group cross sections (instead of the usual 10-group) to treat the moderated spectrum effects adequately. Most of the 28-group data are available from the previous work, so the principal task is to prepare heterogeneity and streaming corrections in the finer structure. Some calculations have been carried out with 10-group cross sections to verify the models to be used for the 28-group DTFX and 2DB calculations and to study the heterogeneity effects of the axial blanket loadings (which had previously been ignored). In the normal dry case, it appears that blanket cell arrangement (plate type loading) has a negligible net reactivity effect. However, with the CH₂-flooded void channels, accounting for multiple scattering in hydrogen may affect the blanket flooding worth by about 25%. the overall core plus blanket zone flooding worth was calculated to within 5% of the measured result in the 10-group analysis for the all-plate configuration.

2.1.3.1.2. Shielding Experiments

Definition of measurement configurations for the second phase (i.e., exit shield phase) of the plenum streaming experiment, and in particular, inclusion of control assemblies in the exit shield measurements, continued. The current CCFR control assembly design has dedicated control assemblies containing no fuel rods. The steel-clad boron carbide control rods are

arranged in a triangular lattice within a round shroud tube. The area requirement of the shroud tube leaves only a small space for exit shielding in the dedicated control assemblies. Therefore, with a fully inserted control rod assembly, a large streaming path can exist to the upper plenum.

The exit shield experiment phase will consider two types of control assemblies:

1. An assembly without exit shielding and with maximum volume utilized for control.
2. An assembly which eliminates some control volume and has a round shroud tube, permitting some shielding on the periphery of the assembly duct, and a boron carbide control rod follower.

Table 2-9 lists the primary exit shield configurations and control rod positions.

With the rod inserted, leakage can occur into the upper parts of the control assembly from the upper axial blankets of the adjacent fuel assemblies. The magnitude of this leakage is dependent upon the distance of rod insertion, so that rod position is an experimental parameter. The largest leakage, and thus upper plenum source, occurs when the rod is fully inserted. Rod positions during plant operation depend upon rod withdrawal sequence (e.g., single rod, single rod bank, or multiple rod bank withdrawal). A three-rod-bank withdrawal sequence (six rods) is preferred, since it minimizes the increase in radial peak-to-average power due to the presence of control rods. For this withdrawal sequence, the rods are inserted X% distance into the core at beginning of equilibrium cycle (BOEC), which corresponds to the largest leakage source.

TABLE 2-9
EXIT SHIELD CONFIGURATIONS

Seven fuel assemblies

Six fuel assemblies and one control assembly

Without Exit Shielding

Rod fully inserted
Rod X% inserted(a)
Rod withdrawn

With Exit Shielding

Rod fully inserted
Rod X% inserted(a)
Rod withdrawn

(a)X% = percent of insertion for rod inserted
the greatest distance at the beginning of the
equilibrium cycle.

2.1.3.2. Methods Development.

2.1.3.2.1. Core Performance Thermal-Hydraulic Methods Development

There was no activity on this subtask during this quarter.

2.1.3.2.2. Rod Thermal Mechanical Analysis.

Adaptation of the LMFBR control rod analysis code CONROD (Ref. 2-21) to GCFR control assembly analysis was begun. The original version of this code handles only wire-wrapped, liquid-metal-cooled assemblies with geometries similar to the fast flux test facility (FFTF) control assemblies. The code also requires thermohydraulic input, which for the GCFR control assembly can be determined only after some CONROD calculations have been completed. The following modifications were made:

1. Coolant was changed from liquid metal to helium.
2. Existing wire-wrap correlations were revised to those for GCFR wire-wrap designs and include provisions for grid-spaced rod bundles.
3. Changing power level and power profile (and resulting coolant flow requirements) with changing axial position of the control assembly were accounted for.
4. A set of hot spot, hot channel factors was calculated for the control assembly.

These modifications will be carried out in two steps:

1. The physics and thermohydraulic calculations involved in the change from a liquid metal to a helium coolant will be done without changing the geometry or existing calculation scheme.

2. The calculation scheme will be revised to handle the GCFR control assembly design.

So far, the coolant has been changed from liquid metal to helium, and the relevant wire-wrap correlations for friction factors and Stanton numbers have been replaced.

2.1.3.2.3. Core Physics Methods Development

During 1980, primary emphasis will be placed on

1. Improving the mass flow and fuel management calculational techniques for conceptual design scoping studies and analysis.
2. Using the new spectrum code MICROX-2 (Ref. 2-22).
3. Verifying and freezing a production version of the 2DB code (Ref. 2-23) for use in design calculations for licensing.

The MICROX-2 fast data tape preparation code FDTAPE was revised to reduce the amount of data required for the inelastic and $n,2n$ transfer arrays. The data size reduction was accomplished by completely rewriting the SQUEEZ subroutine to perform parabolic fitting of the natural logarithm of the inelastic and $n,2n$ transfer arrays. A new GCFR critical-oriented MICROX-2 fast data tape containing data for 67 nuclides was prepared and successfully tested for GCFR production usage. This new tape requires only 5169 storage locations to be read and used in MICROX-2. Such a data tape would have required over 13000 MICROX-2 storage locations with the old version of FDTAPE. It is now possible to prepare fast data tapes for use in MICROX-2 which contain data for a large number of nuclides without requiring excessive amounts of fast memory in MICROX-2. This is important because the temperature and dilution dependence of fast data in MICROX-2 means that multiple sets of fast data will be required for important nuclides.

The MICROX-2 code was revised to read the new exponentially fitted fast data tape and to allow the use of 27 fast and 2 thermal broad-energy groups. The 28 broad-group version requires 109K of UNIVAC-1110 memory. A directly comparable version allowing 10 fast and 5 thermal broad-energy groups requiring 63K was also prepared, and both versions were successfully tested. Revision (splitting) of the MICROX-2 labeled common blocks was necessary to exceed 65K without the FORTRAN computer tables.

A new GCFR-oriented GG data tape was prepared for use in MICROX-2. Most of the fast data sets were deleted because they are now supplied by a separate tape prepared by the FDTAPE code. Additional thermal data sets were added to ensure compatibility with the new 67-nuclide fast data tape.

The resonance region (GAR data) portion of the MICROX-2 code was also modified to use the new delta function synthetic scattering kernel parameter. The interim version has been using the same synthetic scattering kernel parameters as the original MICROX code (Ref. 2-24). More modest changes in computed fluxes, cross sections, and eigenvalues occur when the new parameters are used as shown below.

<u>Test Problem</u>	<u>k_{∞}</u>		<u>k_{eff}</u>	
	<u>Old Scattering</u>	<u>New Scattering</u>	<u>Old SK(a)</u>	<u>New SK</u>
Fort St. Vrain	1.10089	1.10277(+0.00188)	0.97201	0.97361(+0.000160)
ZPR-6-7	1.27899	1.27905(+0.00006)	1.01522	1.01535(+0.000013)

(a)SK = synthetic kernels.

The increased eigenvalues are due to slightly smaller (about 0.5%) capture cross sections for the low-lying resonances in the Th-232 and U-238 with the new SK results. The eigenvalue changes are much smaller in the ZPR-6-7 test problem than in the Fort St. Vrain test problem because low-lying resonances are not very important in fast reactors. The changes in computed fine group fluxes caused by the use of the new SK scattering

parameters are much larger in the ZPR-6-7 than in the Fort St. Vrain test problem (9% versus 0.2 % maximum changes). This is because the $\sum_a/\sum_s$ ratio is much larger in the ZPR-6-7 case and the original MICROX SK scattering yields fluxes do not quite return to the exact flux value after passing through an energy region with a high $\sum_a/\sum_s$ value.

The new GCFR-oriented MICROX-2 data tapes are being used to reproduce selected GGC-5 (Ref. 2-25) calculations for phase 3 of the GCFR critical experiments.

The overall strategy for using subregions in 2DB has been formulated. Little detailed programming work has been completed, and a limited effort was spent in defining the types of feed control and enrichment searches needed in 2DB if it replaces FEVER (Ref. 2-26) for GCFR fuel cycle analyses.

2.2. REACTOR CORE ANALYSIS AND DESIGN

The conceptual design of various mechanical core subassemblies was begun. Emphasis is being directed toward costs and ease of manufacture and assembly of components.

2.2.1. Core Nuclear Analysis

During 1980, a core layout will be determined for the demonstration plant conceptual design. Fuel loading, fuel zoning, and a control rod program scheme will be established. The time-dependent radial and axial power, flux, and burnup distributions will be calculated, and a preliminary verification of shutdown margin and reactivity coefficients will be made. In addition, the conceptual design for the core-related and reactor internal shielding will be selected. Fluence dose and heating rates outside the core will be calculated and compared with design limits to establish the adequacy of the shield configuration.

2.2.1.1. Core Physics Analysis.

2.2.1.1.1. Multidimensional Depletion Analysis

Previous work on characterizing the power distribution for the 400-MW(e) two-loop demonstration plant indicated that further improvements in fuel and control rod strategies are necessary. High beginning of cycle (BOC) 2 radial power peaking (~ 1.44) and low equilibrium cycle reactivity were considered along with the effect of reduced equilibrium cycle power peaking and increased power shape stability on core performance. The primary loop design dictated a reduction in the uncertainty band about the nominally peaked power distribution. Consequently, an attempt was made to model core power peaking more explicitly.

FEVER calculations determined the equilibrium cycle feed that would yield 381 full-power days (FPD) (100,000 MWd/kg) and an end of cycle (EOC) k_{eff} of 1.01. To resolve BOC 2 peaking problems due in part to high age peaking (and in part to rod programming), the initial cycle fuel loading was altered. The new scheme provides better distribution of the fissile loading reduction (i.e., from equilibrium loading) necessary to achieve the proper EOC K . This in turn reduces the age peaking in cycle 2 by more evenly distributing cycle 1 burnup.

Control rod programming alternatives were investigated. For these studies, the spatial effect on neutron flux due to rod motion is assumed to be separable. Axial buckling was assumed constant [determined from a rod-out DOT-IV (Ref. 2-27) supercell]. One bank of control rods was used throughout a cycle to compensate for excess reactivity. Bank selection was based on maximum power shape stability (i.e., minimum change in power shape over the cycle) and minimum power peaking. Control rod sequence B (consisting of six peripheral rods) did not perform well. Simultaneous use of all control rods (minus shutdown rods toward the core center) resulted in higher power peaking toward the center than sequence A. When all rods were

used, the impact on axial power shape was greater. Consequently, the nine-rod pattern was used, and the equilibrium cycle radial power peaking was <1.31.

A GAUGE (Ref. 2-28) depletion for the first four cycles will be done using the new control rod programming and fuel management schemes. CALIOP (Ref. 2-29) relative enrichments will be employed as standard fuel, and the average enrichment which yields 381 FPD and an EOC K of 1.01 will be used. Shutdown margin calculations (using GAUGE) will be determined, and control rod B-10 enrichment will be chosen so that the necessary \$27 of reactivity worth is obtained. Control rod enrichment zoning to reduce the maximum worth rod will be examined. The impact of the control rod on axial shape (neighboring fuel assembly axial buckling) will be studied.

The GAUGE power distribution was used in CALIOP calculations. The r-0 hexagonal GAUGE calculation which models core, radial blanket, and control rod assemblies explicitly over time (i.e., average over length) is more accurate than the calculation previously used in CALIOP. CALIOP will set orifices to adequately cool highly peaked power distribution in the equilibrium cycle.

2.2.1.1.2. Control Assembly Heating Rates for 400 MW(e) Two-Loop GCFR Demonstration Plant

Heating rates in B₄C control material were computed at various locations within a dedicated control assembly at the core center of the two-loop 400-MW(e) GCFR demonstration plant. With the control rods fully inserted, the maximum linear heat rate (assuming a B₄C pellet diameter of 14.8 mm) of 111 W/cm occurs at the core midplane in the outermost row of control rods. With the control rods fully withdrawn to 20 cm above the core-upper axial blanket interface, the maximum heat rate at the bottom of a rod in the outermost row is 21 W/cm. These linear powers can be compared with a maximum permissible power of 300 W/cm recommended in Ref. 2-30. For this study, the control material was assumed to be natural B₄C (19.8 wt %

B-10) at a theoretical density of 0.95. When considering enriched B_4C , it can be assumed that the heating rates are directly proportional to the B-10 content of the B_4C .

Neutron fluxes were obtained from DOT-IV seven-assembly supercell calculations, one with the control rod fully inserted and one with the control rod fully withdrawn to 20 cm above the core - upper axial blanket interface. The fuel and control assembly designs were for the 400-MW(e) two-loop demonstration plant with an active core length of 1200 mm, an axial blanket length of 600 mm, and an exit shielding region length of 250 mm.

Table 2-10 lists the volumetric neutron heating rates (at full reactor power) at selected locations with the control rod either fully inserted or fully withdrawn. With the rod fully inserted, the maximum heating rate of 65 W/cm^3 occurs at the core midplane in the outer row of B_4C rods. With the rod fully inserted, the heating rate in the outer row of rods is about 20% higher than that in the central rod over the length of the core. With the rod fully withdrawn, the maximum heating rate of 9.5 W/cm^3 occurs at the lower tip of the rod, which is located 20 cm above the core - upper axial blanket interface. Large radial and axial heating gradients exist in the withdrawn rod owing to rapid attenuation of lower-energy neutrons and spectral shifts in the axial blanket and exit shield region.

2.2.1.1.3. GCFR Doppler Feedback Reactivity Coefficient Analysis

The Doppler effect, or temperature-induced effective broadening of resonance cross sections in the fertile and fuel isotopes, constitutes a significant fraction of the negative temperature feedback reactivity coefficient required for stable operation of a fast reactor. The prompt Doppler feedback effect also determines the turnaround power level in the unlikely event of a severe accidental reactivity excursion. Disassembly calculations for the GCFR have indicated that the total energy yield from an accidental excursion is highly sensitive to the magnitude of the assumed fuel Doppler coefficient. Calculations of the coefficient are fairly straightforward,

TABLE 2-10
BORON CARBIDE VOLUMETRIC NEUTRON HEATING RATES IN DEDICATED
CONTROL ASSEMBLY OF 400-MW(e) TWO-LOOP DEMONSTRATION PLANT

Control Rod Position	Axial Location	Neutron Heating Rate (W/cm ³)	
		Central Control Rod	Rod in Outer Row
Fully inserted	Core center	55.5	64.7
Fully inserted	30 cm above core center	50.6	59.2
Fully inserted	Top of fuel	37.3	44.3
Fully withdrawn	Bottom of rod	9.54	12.4
Fully withdrawn	Center of rod	0.090	1.29
Fully withdrawn	Top of rod	1.1×10^{-3}	2.4×10^{-3}

but differences in the models and analytical methods used in past studies have produced significant variations in the values of this temperature feedback effect with consequently high uncertainty in the results of the accident analyses.

An extensive analysis was carried out to reevaluate the isothermal fuel Doppler reactivity coefficient in a 300-MW(e) GCFR demonstration plant under normal and accident conditions. The normal reactor configurations studies were (1) a clean (unburned), completely unrodded core (a hypothetical situation) with the geometric specifications of a hot operating state and (2) the same unburned core with all 19 B₄C control rods fully inserted in the core. The accident sequence was (1) a declad, shutdown core with ducts and fuel intact, (2) a declad core with all duct walls slumped, and (3) recritical crumbled core configurations.

The undistorted core configuration adopted for this analysis is the 300-MW(e), 290-kPa core pressure drop design described in Ref. 2-31. The core is composed of three enrichment zones and has a total volume of 4200 liters and an average mixed-oxide fuel enrichment of 16.7% (Pu-239 + Pu-241). Nineteen control and shutdown rods are utilized. The B-10 enrichment in B₄C was set at 66% to provide a calculated target total rod worth of about \$40. The control assemblies contain half the number of fuel rods as the standard core assemblies, with the inner half displaced by the control rod. For the two-dimensional RZ calculational model of the reactor, the fuel and rod sections of the control assemblies were homogenized, giving annular cylinders having six-assembly volumes each for three rings of rods plus a center control assembly. Figure 2-16 shows the reference RZ geometrical model with the hot operating dimensions.

The radial dimensions for all the calculations were those specified for hot operating conditions regardless of the temperature variations assumed for the fuel and associated cross sections. This was done to separate the Doppler effects for the core from the thermal expansion effects. No

Fig. 2-16. Cylindrical bundle for reference 300-MW(e) GCFR for hot operating conditions

temperature variation was assumed for the radial blanket, and thus no accounting was made of the blanket UO_2 -Doppler feedback contribution.

For the unrodded configuration, the rods and their enclosing shrouds were assumed to be completely out of the reactor, thus, the composition for the rod assemblies was a smear of the fuel (UO_2 or steel rods) and its cladding, the inner and outer duct steel, and the void channel helium. In the rodded configuration, the core sections of the rod assemblies included B_4C , cladding, and shroud, and the upper blanket section included steel followers; the lower blanket portion remained a smear of UO_2 ducts and void channel.

After initiation of cladding melting in a hypothetical loss of shutdown cooling accident, the core length of the distorted core models was increased to represent the fuel column expansion to 2000 K; this was the modelling prescribed for the previous studies, which included the effects of expansion on the recriticality potential. In the accident progression, other material heat-ups and expansions with relocations of molten steel and crumbled fuel were allowed, but the prescribed material densities preserved the total mass of the fuel, cladding, and duct materials. For the Doppler calculations, the model specifications at each stage were fixed and only the isotopic cross sections for the core were changed (to the higher temperature set).

For the first distorted core configuration, it was assumed that all the fuel cladding had melted and slumped into the lower axial blanket, plugging the blanket cooling channels to a depth of 20.78 cm. Thus, over the full core height, the steel content was about halved, from 18.7% by volume in the normal rodded core to about 10% in the declad configuration. In the fully rodded configuration, the cladding relocation adds about \$7 in reactivity worth.

In the second distorted core configuration, all the duct walls had melted and slumped, including the inner duct (or guide tube) of the control

assemblies, but not the rod shrouds; the fuel rod columns were still intact. This yielded a region of declad fuel in a void region for most of the core height, with a bottom core region 17.7 cm high containing the fuel sticks at 26 vol % in a layer of molten steel.

The third distorted configuration was the approximate recritical condition assessed by the previous study for a shutdown core with fuel crumbling. For the declad core with duct slumping, it was assumed that the fuel stacks crumbled before their temperature approached the mixed oxide melting point. The fuel chips falling from the upper core regions were assumed to settle and mix uniformly below to yield increasing packing fractions of the fuel chips in the molten steel and steel-voided layers. A 40% packing fraction was determined for recriticality, representing crumbling of about the top 30% of the core fuel. Figure 2-17 presents the calculational model used for this recritical configuration. No radial relocation of fuel by different enrichment type was allowed.

The final distorted configuration consisted of a fuel-crumbled core with a 50% compaction fraction. The 50% packing model was similar to that in Fig. 2-17, except for the different layer thicknesses caused by fuel displacing molten steel. The 50% fuel/50% steel layer at the bottom was 25.41 cm high, and there was a middle 40.64-cm layer of 50% fuel chips in void and a top core region 49.21 cm long which contained only the control rods in helium. Reference 2-30 gives further details on the distorted core configurations.

The calculational procedures followed the standard sequence for designing the normal GCFR core and analyzing performance. The primary difference was in the requirements for generating more sets of cross sections, shielding factors, and streaming factors so that the fuel materials in each of the distorted cases would be appropriately represented, including (1) differing fuel volume fractions accompanied by decreased steel contents)

Fig. 2-17. Recritical configuration for 300-MW(e) GCFR during a hypothetical loss of shutdown cooling accident

in the declad regions or by flooding with molten steel and (2) differing fuel temperatures assumed for the Doppler evaluation (each temperature requires regeneration of the cross section data from the start). In addition, the changing orientations and packing of the fuel pellets influence the shielding of the resonance cross sections, and the spectrum changes associated with the control rod insertions appreciably alter the broad-group average cross sections for the fuel isotopes and B-10.

2.2.1.1.4. Generation of Cross Sections

The GGC-5 code (Ref. 2-25) was used to prepare cross sections in a 10-group energy structure from the basic data in the ENDF/B-4 library. Two sets of cases were run for the undistorted core, one at a fuel temperature of 300 K and the other at 1300 K. In the unrodded set, the two-region GAROL model (Ref. 2-32) prescribed an average enrichment fuel rod in a standard assembly surrounded by a diluent of steel (cladding, ducts, etc.) plus helium around each rod.

For the rodded core, the average cell composition in the GGC-5 problem was that of the homogenized total core content, including the B-10, B-11, carbon, and extra steel introduced with the 19 control rods inserted. In the resolved resonance range (GAROL section), the two-region rod cell modeled the average fuel rod surrounded by the apportioned steel and helium zone, but excluding the B-10 to avoid overshielding effects. For the GAM spectrum calculation in 99 fine groups, the control materials were entered at full density but included shielding factors to provide for the impact on the fine spectrum. All subsequent GGC-5 calculations for the distorted core configurations were modeled in the same fashion, including the control rod materials with the shielding factors used in the evaluation of core average weighting spectra with the rods inserted in whatever fuel/steel/helium layer makeup was considered.

Two sets of GGC-5 configurations were utilized for the slumped-steel configurations with intact fuel one at a temperature of 1300 K, and the

other at 3000 K. For one set the problem modelled a cylindrical fuel rod at 26 vol % surrounded by a near-void region with the outer diluent containing only average control rod steel and B_4C . The other set represented the lower core portion with a similar fuel rod immersed in molten steel.

For the crumbled fuel configuration at 40% and 50% packing fractions, the fuel geometry was assumed to be spherical with a radius equivalent in mean chord length to a stack of two pellets (a completely arbitrary assignment). The GAM solution again included the B-10, B-11, and carbon at the average core concentration, but with the higher fuel concentrations from the assumed compactions. Four new GGC-5 bases were run for the 40% packing stage: for the 40% fuel/60% helium and 40% fuel/60% steel environments at temperatures of 2000 and 4000 K. The 50% packing configuration calculations were run in the previous recriticality study with temperatures of 2000 and 3000 K.

For all the outer reactor regions, i.e., radial blanket, normal axial blanket, and plugged axial blanket, the cross sections used were those derived for the previous recriticality studies. Hot operating temperatures were assumed for the blanket UO_2 .

2.2.1.1.5. Control Rod Shielding Factors

Shielding factors were calculated for the B_4C cross sections for the normal and distorted core axial zone environments. The one-dimensional model for the DTFX (Ref. 2-33) transport theory problems, which use 10-group P_1 cross sections and S₈ quadrature, prescribed a rod cell of four B_4C rings within a half-assembly annulus of fuel for the control assembly. This was surrounded by a six-assembly ring of standard fuel assemblies. The input cross sections were extracted from the appropriate distorted or normal zone GGC-5 case cited above. The derived group shielding factors express the ratio of average flux within the B_4C to average flux over the central control assembly. Advantage factors were also derived for the fuel content

of the control assembly to reflect the relatively higher flux relative to the average assembly flux.

Rod cell calculations were made utilizing cross sections from the new GGC-5 runs to derive the control assembly shielding factors in each of the normal and distorted core layers. Five sets of two DTEX runs each were made for the rod cells:

1. The normal shutdown core.
2. The 26% fuel in helium declad core.
3. The 26% fuel in molten steel layer.
4. The 40% fuel in helium layer.
5. The 40% fuel in molten steel layer.

2.2.1.1.6. Rod Lattice Streaming Effects

Bidirectional diffusion coefficient modifiers were employed to effect preferential streaming through the coolant channels in the axial direction. The correction factors, as used in 2DB, are provided by the PINDF3 code (Ref. 2-34) based on the theory of Benoist (Ref. 2-35). These factors were recalculated for the cases in which fuel pellets were still intact in full-height columns but with cladding and duct slumped away. For the molten steel-plus-fuel layer and the distorted cases in which fuel had crumbled, it was assumed that the streaming paths were obliterated and diffusion constants for a homogenized medium applied. Since the adopted configuration models were the same as they are for the stage 1 recriticality study, the previously derived modifiers were used.

2.2.1.1.7. Void Region Diffusion Coefficients. The void regions produced in the upper core by the descent of crumbled fuel require special treatment to better represent the effect of induced leakage on reactivity. As in the crumbled fuel recriticality study, the method reported in Ref. 2-36 was used. The procedure is a crude approximation for the void dimensions involved, but it is better than none at all because of the limitations of diffusion theory. For the compacted fuel case, it was assumed that the upper core void zones (interrupted by the control rod rings) contained helium and a steel vapor content 0.25% by volume. The diffusion coefficient in the Z direction for the void layer height Z is approximated as

$$D_Z = [D_1 e^{-\Delta Z/4D_0} + D_0 (1 - e^{-\Delta Z/4D_0})] ,$$

where D_0 is the normal void diffusion coefficient (due to helium and steel), and D_1 is the average coefficient on either side of the layer. In 2DB, the pseudocoefficient is produced by entering the modifiers $M = D_Z/D_0$ by group for the void zone.

2.2.1.1.8. Diffusion Theory Eigenvalue Calculations

The diffusion theory code 2DB (Ref. 2-23) was used to calculate eigenvalues for R-Z models of the progression of configurations, starting with the unrodded reference case to the crumbled cores situated on top of a plugged axial blanket. In the distorted cases, the conservative, highly unlikely assumption was made that dislocation and downward motion of the slumped steel and crumbled fuel took place uniformly across the entire core, giving distinct layers of uniform average material compositions of different fuel-plus-steel, fuel-plus-void, and void regions. However, the three radial fuel enrichment zones were retained. Once the fuel cladding refroze in the upper part of the lower axial blanket, no further distortion of the outer reactor regions was assumed. The reactivity change from the fuel Doppler effect at various stages was derived by merely switching cross section sets without temperature-induced changes of model density and

geometry. Table 2-11 presents the results of the 2DB calculations for the normal core configurations with and without the 19 control rods inserted. Table 2-12 gives the 2DB results for the distorted core configurations representing the crumbled fuel scenario. For the purpose of comparison, evaluation of the Doppler coefficients assumes a $T^{-1.00}$ relationship for the Doppler feedback reactivity.

The factor -3.76×10^{-3} in Table 2-11 for the clean, unrodded, normal core is consistent with past Doppler feedback evaluations for 300-MW(e) GCFR cores. It appears that for the fully rodded, undistorted core, the addition of the control poison cuts the Doppler coefficient in half. However, this is the result of inappropriate use of the unrodded core spectrum collapsed 10-group cross sections. More rigorous use of reevaluated cross sections for the rodded core gives a value of -2.92×10^{-3} , which is about 77% of the normal core Doppler coefficient. An unburned core was assumed for all cases (no fission products were included).

Table 2-12 shows that decladding the fuel and draining the molten steel from the core significantly hardens the neutron spectrum, reducing the Doppler factor another 33%. This rodded-core, declad-fuel coefficient, -1.94×10^{-3} , is 52% of the unrodded normal core factor. Further slumping of steel because of melting of all the assembly ducts reduces the Doppler coefficient another 25%, not as much of a decrease as that experienced with decladding, because the steel remains mixed with the fuel material at the bottom of the core.

For the recritical stage, at 40% packing of the crumbled fuel, there is only a slight reduction of the core-average isothermal Doppler coefficient, to -1.498×10^{-3} . This implies that once steel relocation has stopped and crumbling fuel chips settle into the lower core (giving higher fuel densities but also remixing with steel), the Doppler factor has stabilized. Slightly different treatment of the $B^{4}C$ self-shielding factors was used for the 50% packing, crumbled fuel case. This was done during stage 1

TABLE 2-11

DIFFUSION THEORY CALCULATIONS FOR UNDISTORTED CONFIGURATIONS

Reactor Configuration(a)	Cross Section Generation(b)	Isothermal Fuel Temperature (K)	2DB Calculated k ($\pm 2 \times 10^{-5}$)	Doppler Coefficient(c)
Hot operating condition, clean, unrodded(d)	Clean core (B-10 at infinite dilution)	300 1300	1.05553 1.04942	-0.00376
Hot shutdown condition, clean, 19 B ₄ C rods installed (66% enriched)	Clean core (B-10 at infinite dilution)	300 1300	0.89484 0.89257	-0.00194
Hot shutdown condition, clean, 19 B ₄ C rods installed (66% enriched)	Rodded core (B-10 at average density in shut-down state)	300 1300	0.089763 0.89419	-0.00292

(a) For fixed-region geometry and material density specifications.

(b) GGC-5 cases for core.

(c) $\Delta / [\ln(T_2/T_1)]$.

(d) Clean means no fission product accumulation.

TABLE 2-12
2DB CALCULATIONS FOR CCRE DOPPLER COEFFICIENTS IN
DISTORTED CORE CONFIGURATIONS

Reactor configuration(a)	Cross Section Generation(b)	Isothermal Fuel Temperature (K)	2DB Calculated k ($\pm 2 \times 10^{-5}$)	Doppler Coefficient(c)
Declad-core, fuel and duct walls intact, fully rodded (19 rods in), cladding as blanket plug	Declad, ducts slumped (26% fuel) in helium), shutdown B-10 density	1300 3000	0.91501 0.91365	-0.001945
Declad, ducts slumped to core bottom, fully rodded, fuel intact	26% in helium, 26% fuel in molten steel (plus shutdown B-10 density)	1300 3000	0.94529 0.94417	-0.001501
Crumbled fuel at 40% packing fraction (recritical with all rods in)	40% fuel in helium, 40% fuel in molten steel (plus shutdown B-10 density)	2000 4000	1.01572 1.01455	-0.001498
Crumbled fuel at 50% packing fraction, fully rodded	50% fuel in helium, 50% fuel in molten steel (shutdown B-10 density)	2000 3000	1.04270 1.04214	-0.00127

(a) Specific geometry and material densities

(b) GGC-5 cases for fuel region.

(c) $\Delta P / [\ln(T_2/T_1)]$.

recriticality analysis. Even so, the Doppler coefficient obtained, -1.27×10^{-3} , is consistent with the results of this analysis.

The Doppler coefficient of -3.76×10^{-3} for the range 300 to 1300 K for a clean (unburned), unrodded 300-MW(e) GCFR core is in good agreement with a previous value of -3.85×10^{-3} for a similar 300-MW(e) GCFR (low core pressure drop design). The result is consistent with an ANL analysis for an older design with a 3200-liter, 4-zone core of 17.4% average enrichment. The ANL Doppler coefficient was -3.285×10^{-3} for a temperature range of 1300 to 3000 K for a 300-MW(e) core in an unrodded, unpoisoned state. The 14% lower value can be rationalized as due to a 5% lower fertile-to-fission ratio accompanied by a 5% to 10% lowering effect caused by the higher temperature range (estimating a temperature exponential factor of between -1.05 and -1.10).

It thus appears that for the clean configuration, the isothermal Doppler coefficient for average cold to hot temperatures is in the range $-3.5 \pm 0.3 \times 10^{-3}$, or about half that predicted for an LMFBR core with a similar power rating. This has been verified by small-sample Doppler-worth measurements in both Clinch River breeder reactor (CRBR) mock-ups and GCFR benchmark critical experiments, in which the agreement between calculated and measured sample Doppler effects has been good (Ref. 2-37).

For kinetic calculations of hypothesized accident configurations in which there is melting and relocation of the fuel cladding and assembly duct steel, it appears that a Doppler factor of -1.50×10^{-3} would be sufficiently conservative, since this is approximately the minimum value obtained for complete steel slumping in a fully rodded, unburned core. Coefficients for a poisoned core or for more advanced accident conditions will require further calculations. Based on the results in Table 2-12 for the distorted clean core, it does not seem too unreasonable that a molten fuel configuration could have a Doppler coefficient as low as -1.0×10^{-3} for an EOC core, depending on enrichment, relative fuel/steel separation, and fission product

retention. As a lower limit, a value of -1.0×10^{-3} would agree with that used by ANL in disassembly calculations.

As the design of the GCFR demonstration plant becomes more firmly established, reevaluation of the Doppler heat-up reactivity effect will be required for prediction of the operational power coefficient and for safety analyses. It is recommended that a finer group structure be employed for these calculations (20 to 30 groups versus 10 groups) to avoid the requirement for separate sets because of varying poison concentrations. A basic multigroup cross section set should be generated for the undistorted core using a reference composition which includes average fission product and control material content at midequilibrium cycle. New accident analyses would again require regeneration of the cross sections for distorted configurations.

2.2.1.2. Shielding Physics Design and Analysis. The major shielding effort during the last quarter was on the design and preliminary analysis of new upper and lower plenum shielding configurations compatible with natural circulation (Ref. 2-38). During this quarter, preliminary DOT-IV (Ref. 2-39) two-dimensional radiation transport calculations for two proposed flow-through shields were completed, and preliminary shielding physics data were obtained for the GCFR central closure.

Several flow-through shields compatible with natural circulation were studied. Simplified models of the two shield configurations (herringbone and crescent designs) were used in two-dimensional radiation transport calculations performed with the DOT-IV code. In both cases, the GA 36-group coupled neutron/gamma ray cross sections from ORNL were used with S_8 angular quadratures and P_3 anisotropy. However, to reduce computation costs, only the 26 neutron groups were given 10 iterations to a point-flux convergence of 0.001.

Figure 2-18 shows the simplified model of the herringbone shield in X-Y coordinates, and Figs. 2-19 through 2-21 present two-dimensional isoflux plots of (1) total flux; (2) thermal flux ($E < 2.38$ eV); (3) epithermal flux ($0.1 \text{ MeV} > E > 2.38$ eV); and (4) second fast flux ($1.0 \text{ MeV} > E > 0.1 \text{ MeV}$).

Figure 2-22 shows the simplified model material zones by interval for the arc shield and models the material in the right half of the complete shield except that the duct to the steam generator and CAHE is omitted. The θ intervals represent angles from 0 to 180 deg, and the radial intervals represent the radii from $r = 281$ cm, the upper plenum source radius, to $r = 410$ cm, which includes about 16 cm of concrete. Figures 2-23 through 2-28 present the isoflux plots of (1) total flux; (2) thermal flux ($E < 2.38$ eV); (3) epithermal flux ($0.1 \text{ MeV} > E > 2.38$ eV); (4) second fast flux ($1.0 \text{ MeV} > E > 0.1 \text{ MeV}$); and first fast flux ($E > 1.0 \text{ MeV}$).

Since total neutron flux will be the material damage criterion until damage function analysis becomes more accurate, the total neutron flux plots will be emphasized.

Clearly, the crucial aspect of the flow-through shield (Fig. 2-18) is neutron streaming through a relatively wide gap. With an essentially isotropic source in the plenums, the use of X-Y coordinates with the gap directed in an equivalent radial direction is a reasonable approximation of the proposed configuration. In addition, the right-angle corners also approximate the curved corners as far as neutron transport is concerned. The layers of $B_4C + C$ along both sides of the streaming gap minimize back scattering of thermal and epithermal neutrons into the streaming gap.

Figure 2-19 presents the isoflux plot for total neutron flux. The total normalized fluxes at points A, B, C, and D are 3.7×10^{-2} , 6.2×10^{-5} , 9.6×10^{-5} , and $4.15 \times 10^{-2} \text{ n/cm}^2\text{-s}$, respectively. At the source radius (points A and D) and the liner radius (points B and C) the maximum flux does not occur along the centerline of the gap. Backscattered neutrons from

Fig. 2-18. Simplified two-dimensional model of herringbone shield in X-Y plane coordinates

Fig. 2-19. Isoflux plot of total neutron flux (normalized) for herringbone shield.

Fig. 2-20. Isoflux plot of thermal flux ($E < 2.38$ eV) (normalized)
for herringbone shield

Fig. 2-21. Isoflux plot of epithermal flux ($0.1 \text{ MeV} > E > 2.38 \text{ eV}$)
(normalized) for herringbone shield

Fig. 2-22. Material zones by angular and radial interval in arc shield

Fig. 2-23. Isoflux plot of second fast flux ($1.0 \text{ MeV} > E > 0.1 \text{ MeV}$) (normalized) for herringbone shield

Fig. 2-24. Isoflux plot of total neutron flux (normalized) for arc shield

Fig. 2-25. Isoflux plot of thermal flux ($E < 2.38\text{ eV}$) (normalized) for arc shield

Fig. 2-26. Isoflux plot of epithermal flux ($0.1 \text{ MeV} > E > 2.38 \text{ eV}$) (normalized) for arc shield

Fig. 2-27. Isoflux plot of second fast flux ($1.0 \text{ MeV} > E > 0.1 \text{ MeV}$) (normalized) for arc shield

Fig. 2-28. Isoflux plot of first fast flux ($E > 1.0$ MeV)
(normalized) for arc shield

the stainless steel can account for the increased scalar flux on either side of the gap at the source radius. However, the reason that the flux at the liner radius is at a maximum outside the gap streaming path is not understood. It could be a geometrical ray scattering effect.

Even though these results are preliminary, the effectiveness of the herringbone flow-through shields is demonstrated. The changes in minimum flux from the source radius to maximum flux at the liner radius indicate a flux reduction by a factor of about 400, where a factor of about 100 is sought in the upper plenum. However, in the lower plenum, a flux reduction of 500 to 1000 may be required. If so, this shield can easily be made thicker. This may not be necessary because preliminary indications from the ORNL upflow GCFR calculations are that the fluences in the upper and lower plenums at the shield radius will be less than 10^{21} and 10^{22} nvt, respectively. Figures 2-20, 2-21, and 2-23 show that the flux in the shield is dominated by epithermal flux, and Figs. 2-21 and 2-23 distinctly indicate the higher fluxes at the liner radius on either side of the streaming gap.

These preliminary calculations demonstrate that both the proposed herringbone and arc flow-through shields are very effective and that the chevron variation of the herringbone type flow-through shield will also be effective.

Because of the difficulty of scaling to a commercial GCFR, the arc shield will not be considered for the demonstration plant. Nevertheless, the results were interesting and informative and the first known calculation for such a shield configuration. Point A in Fig. 2-24 is the slot opening to the arc shield. Coolant flow and neutron streaming are counterclockwise between the shield structures to the duct located in the point B direction. The flux reduction from point A to the liner near point B is a factor of about 1000, and the 3.16×10^{-6} flux contour near point B approximately coincides with the rear surface of the interior of the two radial shield structures. This indicates that in this configuration, the flux at B is mainly determined by the interior shield structure, with only a secondary

scattered streaming contribution along the streaming path. This shield configuration is clearly very effective. The curved surfaces effectively remove the uncollided flux at B from A, leaving only a secondary scattered contribution. Figure 2-25 shows the thermal flux buildup in the concrete near point B. This same result has been obtained in one-dimensional calculations at the core midplane. Figure 2-26 shows that epithermal flux dominates the total flux.

Shielding core model C is the core model to be used in future shielding analysis at ORNL and GA. It supercedes the previous shielding core model A (SCMA), (Ref. 2-40). The model represents the two-loop demonstration plant core. The nuclide densities correspond to end of equilibrium cycle (EOEC) values and were obtained from FEVER/2DB depletion studies (Ref. 2-41). Maximum out-of-core flux levels occur at EOEC.

The primary differences between the old and new model are as follows:

1. Axial blanket length is increased from 450 to 600 mm.
2. Total number of core assemblies is increased from 127 to 169.
3. Number of radial blanket rows is increased from 2 to 3.
4. Dedicated control assemblies are incorporated.
5. Core pressure drop is reduced from 0.289 to 0.162 MPa.
6. Radial blanket shuffling scheme is changed from direct outward shuffle to a modified scheme in which fresh assemblies are loaded into row 3, shuffled to row 1 after 2 years, and shuffled to row 2 after 2 more years.

Table 2-13 gives the design parameters for the core and fuel, control, and blanket assemblies, and Fig. 2-29 shows the R-Z model and homogeneous

TABLE 2-13
MAJOR DESIGN PARAMETERS^(a) (HOT DIMENSIONS) FOR THE 169-ASSEMBLY
400-MW(e) GCFR TWO-LOOP DEMONSTRATION PLANT

Reactor core

Reactor power [MW(t), MW(e)]	1076, 400
Primary system pressure (MPa)	10
Peak linear rating (kW/m)	31.83
Core inlet temperature (°C)	302
Core outlet temperature (°C)	547
Radial blanket	3 rows
Axial blanket length (mm)	600
Number of fuel assemblies	150
Number of control assemblies (dedicated)	19
Number of radial blanket assemblies	156
Number of core enrichment zones	3
Core height (mm)	1210
Core diameter (cylinder) (mm)	1407.11
Core ΔP (MPa)	0.162
Fuel cycle length (full-power days)	381.0

Fuel assembly

Cross section	Hexagonal
Active core height (mm)	1210
Number of fuel rods	264
Number of hanger rods	6
Number of instrumentation rods	1
Fuel rod spacer type	Spacer grid
Number of spacer grids	12
Across flats i.d. (mm)	187.43
Across flats o.d. (mm)	195.52
Duct wall thickness (mm)	4.05
Fuel rod pitch (mm)	11.19
Rod-to duct gap (mm)	2.49
Fuel assembly pitch (mm)	206.16

Fuel rod

Outside diameter (mm)	8.07
Outside root diameter (mm)	7.81
Inside diameter (mm)	6.98
Cladding root thickness (mm)	0.38
Cladding material	20% cold-worked 316 stainless steel
Fuel and blanket pellet o.d. (mm)	6.84
Fuel and blanket pellet-to-cladding diametral gap (mm)	0.14
Fuel length (mm)	1210
Fuel material	Mixed PuO ₂ -UO ₂
Fuel material planar smear density (% of theoretical density)	85.4
Axial blanket length (each) (mm)	600

TABLE 2-13 (continued)

Fuel Rod (continued)

Axial blanket material	Depleted UO ₂
Axial blanket material planar smear density (% of theoretical density)	90.0
Blanket assembly	
Number of blanket rods	61
Outside diameter (mm)	21.70
Inside diameter (mm)	20.70
Cladding material	20% cold-worked 316 stainless steel
Blanket length (mm)	2170.0
Blanket material	Depleted UO ₂
Blanket material smear density (% of theoretical density)	90.0
Spacer system	Wire wrap
Wire wrap diameter (mm)	1.45
Wire wrap pitch (mm)	300.0
Control assembly	
Number of dedicated control assemblies/core	19
Poison height (mm)	1200
Number of control rods/assembly	55
Number of hanger rods/assembly	6
Control rod spacer type	Spacer grid
Number of spacer grids	5
Duct material	20% cold-worked 316 stainless steel
Control rod channel material	20% cold-worked 316 stainless steel
Control rod channel across flats i.d. (mm)	168.0
Control rod channel across flats o.d. (mm)	176.0
Control rod channel wall thickness (mm)	4.0
Control rod pitch (mm)	20.9
Rod-to-rod gap (mm)	4.1
Rod-to-channel gap, (mm)	3.2
Rod-to-channel gap (% of rod-to-rod gap)	78
Control rods	
Number per control assembly	55
Length (mm)	1300
Outside diameter (mm)	16.8
Inside diameter (mm)	15.8
Cladding thickness (mm)	0.5

Fig. 2-29. Region diagram for R-Z geometry model used for 2DB calculations

region numbers used in the 2DB equilibrium cycle depletion calculation. Figure 2-30 shows a one-third core layout; there are 3, 3, and 6 regions in the core, axial blanket, and radial blanket, respectively. Table 2-14 gives fuel, structure, and helium volume fractions assuming all control rods are withdrawn from the axial blanket. Tables 2-15 and 2-16 give the homogeneous region nuclide densities for the important heavy metals (at EOEC) and the nonburnable isotopes, respectively.

2.2.2. Core Thermal-Hydraulic Analysis

2.2.2.1. Fuel Assembly.

2.2.2.1.1. Fuel Assembly Peak Cladding Temperatures During a DBDA Transient

The thermal-hydraulic results predicted by the SCRIMP subchannel analysis code and the FASTRAN system code under DBDA transient conditions (Ref. 2-42) have been compared. The FASTRAN systems code predicts plant performance during steady-state and transient operating conditions. It represents the core assemblies as "unit cells," and the calculated results must be calibrated against assembly subchannel analysis codes to verify maximum cladding temperature predictions. A benchmark program has been conducted to account for differences in the calculational procedures of the two codes and to determine the effects of coolant flow redistribution, cladding conduction, radiation, and duct wall heating on the location and level of maximum fuel assembly cladding temperatures during a DBDA transient. Geometric and performance data for fuel assembly design from Ref. 2-31 were employed. The only modification involved moving the hanger rods in the corners to the interior of the bundle and replacing them with fuel rods.

The time at which peak fuel rod cladding temperatures occur during a DBDA transient was determined using FASTRAN (Refs. 2-43, 2-44). The fuel rod bundle was modeled as six axial sections; four sections represented the fueled region and two the axial blankets. To avoid problems associated with

Fig. 2-30. Core layout for 400-MW(e) two-loop GCFR demonstration plant (150 fuel assemblies, three-row radial blanket, 19 dedicated control assemblies, one-third of core)

TABLE 2-14
FUEL, STRUCTURE, AND HELIUM VOLUME FRACTIONS IN HOMOGENIZED
REGIONS OF TWO-LOOP DEMONSTRATION PLANT

	Volume Fraction		
	Fuel	Structure	Helium
Core zone 1	0.24716	0.16150	0.59134
Core zone 2	0.22844	0.15479	0.61677
Core zone 3	0.27920	0.17298	0.54782
Axial blanket (average)	0.24781	0.16174	0.59045
Radial blanket	0.45000	0.11260	0.43740

TABLE 2-15
NUCLIDE DENSITIES FOR IMPORTANT HEAVY METALS AT
END OF EQUILIBRIUM CYCLE

Region	Region Number	Nuclide Density (atom-barn ⁻¹ -cm ⁻¹) ^a						
		U-235	U-238	Pu-238	Pu-239	Pu-240	Pu-241	Pu-242
Core	1	5.7174-6(a)	4.0444-3	6.8386-6	5.8386-4	1.9366-4	6.0909-5	2.4717-5
	2	6.1283-6	4.0258-3	8.7796-6	6.7871-4	2.2805-4	7.7753-5	2.9343-5
	3	6.9210-6	4.1795-3	1.3347-5	9.5487-4	3.1804-4	1.1827-4	4.0792-5
Axial blanket	4	9.2623-6	5.3016-3	N(b)	9.4050-5	2.3548-6	N	N
	5	9.8478-6	5.4500-3	N	7.3865-5	1.3878-6	N	N
	6	1.1328-5	6.0503-3	N	5.4853-5	6.8460-7	N	N
Radial blanket	7	1.6955-5	9.6645-3	N	1.5705-4	3.0191-6	N	N
	8	1.5618-5	9.5649-3	N	2.3573-4	7.0731-6	N	N
	9	1.9191-5	9.8161-3	N	2.8490-5	1.5630-7	N	N
	10	1.8862-5	9.7960-3	N	4.9653-5	3.3542-7	N	N
	11	1.8403-5	9.7664-3	N	7.6832-5	7.8410-7	N	N
	12	1.9671-5	9.8387-3	N	8.4495-6	1.4614-8	N	N

(a) 5.7174-6 = 5.7174×10^{-6} .

(b) Negligible.

TABLE 2-16
NUCLIDE DENSITIES FOR NONBURNABLE ISOTOPES

Region	Region Number	Nuclide Density [(atom-barn ⁻¹ -cm ⁻¹)]						
		He	O	Cr	Mu	Fe	Ni	Mo
Core zones	1	4.9709-4(a)	1.0337-2	2.6164-3	2.5470-4	9.0064-3	1.7875-3	2.0258-4
	2	5.2382-4	1.0574-2	2.7664-3	2.6931-4	9.25229-3	1.8901-3	2.1420-4
	3	4.6051-4	1.1677-2	2.8024-3	2.7281-4	9.6466-3	1.9147-3	2.1698-4
Axial blanket	4-6	5.0646-4	1.1088-2	2.6737-3	2.6028-4	9.2032-3	1.8268-3	2.0702-4
Radial blanket	Average							
	7-12	3.8193-4	1.9737-2	1.5512-3	1.5101-4	5.3395-3	1.0598-3	1.2010-4

(a) 4.9709-4 = 4.9709 x 10⁻⁴.

transient heat storage effects, sectional coolant flows and temperatures were used to calculate average surface heat flux levels for the steady-state SCRIMP analysis. However, the FASTRAN results must be adjusted to separate fuel rod heating from duct wall heating. The ratio of duct wall volumetric heating to homogenized fuel assembly volumetric heating 354 s after shutdown was found to be 0.48 (Ref. 2-45). Application of fuel and cladding volume fractions indicated that duct wall volumetric heating was 22% of that of a fuel rod and accounted for 5.1% of the total heat generated in the assembly at that time. Table 2-17 presents the resulting sectional heat flux and equivalent linear power levels. Rather than use constant surface heat flux values over each section, the flux was distributed in accordance with a typical axial profile such that the integrated average flux in each section corresponded to the values given in Table 2-17. The fuel assembly was assumed to have no radial power skew.

Figure 2-31 shows the SCRIMP model used. It consists of 57 surfaces and 15 subchannels, sized so that the ratio of interior to peripheral channel flow areas is similar to that of the full bundle. Although lateral energy transport occurred between sections as the flows were redistributed, no turbulent heat or momentum exchange was included. Gaseous conduction was a second-order effect and was also not included (Ref. 2-46). The fuel rod energy was deposited in the center of the rod and allowed to be radially and circumferentially conducted before leaving the rod surface by convection and radiation. No assembly heat loss or gain through the duct wall was allowed. Radiation view factors were converted into Hottel form factors using the method in Ref. 2-47, and a conservative emissivity value of 0.3 was used. Different thermal-hydraulic correlations were used for the corner, wall, and interior subchannels with the techniques presented in Ref. 2-48. These correlations indicate that for the same laminar Reynolds number, the peripheral channel friction factors are lower and the Nusselt numbers higher than the corresponding values for interior channels.

TABLE 2-17
FUEL ROD SURFACE HEAT FLUX AND EQUIVALENT LINEAR POWER(a)

Section	Length(b) (m)	Inlet Temperature(b,c) (°C)	Adjusted Rod Heat Flux (kW/m ²)	Equivalent Rod Linear Power (W/m)
1	0.450	231.3	3.54	80.9
2	0.2848	254.0	35.87	819.7
3	0.2848	399.5	47.69	1089.4
4	0.2848	592.9	48.23	1102.4
5	0.2848	788.5	37.34	853.4
6	0.4498	939.9	4.84	110.6

(a) Unit cell flow rate = 0.3255 g/s; unit cell pressure drop = 5.79 kPa.

(b) From Ref. 2-44.

(c) Outlet temperature = 970.0°C.

FUEL ROD O.D.	= 7.46 MM	CLADDING THICKNESS	= 0.51 MM
FUEL ROD PITCH	= 10.4 MM	DUCT WALL THICKNESS	= 3.9 MM
EDGE SPACING	= 2.4 MM		

Fig. 2-31. SCRIMP fuel assembly model

The SCRIMP code accepts a system mass flow rate and distributes that flow in each section to equalize the pressure drops in the subchannels. Therefore, matching a given pressure drop generally requires three runs. Figure 2-32 shows maximum surface temperatures predicted by SCRIMP for the specified pressure drop. The temperature of the interior rod is essentially identical to that predicted by FASTRAN (981°C, Ref. 2-44). The temperatures increase steadily as the edge of the assembly is approached and peak in the corner subchannel region. The temperature differential between the corner and interior rods also increases with axial distance until the peak cladding temperature location is reached, as shown in Fig. 2-33. At the maximum temperature location, shown in Fig. 2-32, this differential is 249°C. If the hot spot factors of Ref. 2-49 are applied, the hot spot temperature is 1466°C, which is 484°C above the nominal temperature of the interior rod. These temperatures were calculated including the effects of radiation and circumferential conduction. The FASTRAN model predicted a hot spot temperature of 1209°C (Ref. 2-43), which is 257°C lower than that calculated by SCRIMP. The proposed reference design will maintain hot spot temperatures within the PC-5 (faulted) limit (1300°C).

As expected, inclusion of cladding circumferential conduction and surface-to-surface radiation heat transfer modes has a marked effect on temperature and temperature gradient. The normalized data in Table 2-18 show these effects. This table shows that the cladding circumferential temperature gradient is a much stronger function of conduction than of radiation; exactly the reverse is true for the temperature gradient between the corner and interior rods, which is more strongly dependent on radiation. The large magnitude of this gradient is due to the elimination of radiation heat transfer, which increases peripheral temperatures while reducing interior temperatures.

The following conclusions have been reached:

1. When the SCRIMP code simulates the unit cell model of FASTRAN, it produces similar cladding temperatures and pressure drops.

Fig. 2-32. Nominal temperatures (°C) at outlet of fueled section

Fig. 2-33. Cladding axial temperature profiles

TABLE 2-18
EFFECT OF REMOVAL OF RADIATION AND CIRCUMFERENTIAL
HEAT TRANSFER MODES ON CALCULATED TEMPERATURES

	Radiation and Conduction	Conduction, No Radiation	No Radiation or Conduction
ΔT , rod 1 (a)	0	+13	+80
ΔT , rods 1 through 6(a)	0	+113	+125
T , rod 1(b)	0	+17	+20
T , duct(b)	0	+18	+23

(a) Values shown are percentage changes.

(b) Values shown are percentage changes in surface temperature rise from inlet.

2. At the time of peak cladding temperatures in a DBDA transient for the Ref. 2-31 design (with fueled corner rods), the temperature differential between the corner rod and an interior rod is 250°C. The lateral temperature gradient across the corner rod is only 15°C.
3. Large errors are introduced into the analysis by ignoring cladding circumferential conduction and radiation heat transfer.

These conclusions are directly applicable to only the modified Ref. 2-31 design, with its high linear power and core pressure drop. No attempt should be made to extrapolate these results to the current GCFR core designs. These analyses are being repeated for the proposed reference core assembly designs.

2.2.2.1.2. Edge Channel Design Study

To accurately establish the surface temperature profile for a GCFR edge fuel rod in the vicinity of a spacer, a detailed examination was made using twenty-two nodes in COBRA*GCFR (Ref. 2-9) over a two-spacer span for a one-twelfth section of a 271-rod central fuel assembly. The pronounced sawtooth pattern previously observed for diametral temperature difference on an edge rod was confirmed.

Figure 2-34 shows channel flow rate, rod surface heat transfer coefficient, rod surface temperature, and rod diametral temperature difference as a function of rod length. The rod surface heat transfer coefficients sharply increase in the vicinity of a spacer. Because of the considerably greater spacer solidity of the edge channel, its increase in heat transfer coefficient is greater than that for the interior channel, even though the flow rate decreases in the edge channel. Thus, the pattern shown is a result of the local value of the heat transfer coefficient, which is flow dependent, and the local magnitude of the spacer-induced heat transfer enhancement factor, which is spacer solidity dependent. As a

Fig. 2-34. Temperature and flow variation in the vicinity of spacers (channel 40 = interior, channels 55/56 = edge; edge spacing = 65%; rod pitch = 11.1 mm; rod diameter = 8.0 mm; heated length = 1200 mm)

consequence of the heat transfer variation described above, the cladding surface temperatures for both the interior and edge channels decrease (by differing amounts in the vicinity of a spacer) and reach a minima in the middle of a spacer. This produces a pronounced sawtooth profile for rod diametral temperature difference.

Figure 2-35 compares the rod diametral temperature difference obtained using 22 and 5 nodes over the same length. The 5-node curve is typical of that obtained using 22 nodes for the entire rod length. The 22-node curve is more complex than the 5-node curve, but a good overall representation. Whether the observed differences in curve shapes appreciably influence rod bowing is not known.

The main objective of the thermal-hydraulic analysis for the GCFR core is to limit fuel rod diametral temperature difference to a value low enough to ensure that rod bowing will not shorten the desired fuel life. Recent mechanical analyses have shown that the extent of rod bowing is a function of not only the magnitude of the rod diametral temperature difference, but swing as well. COBRA*GCFR runs performed for a one-twelfth section of a 271-rod central fuel assembly rod show that rod diametral temperature difference is low when swing is high and vice versa. Over the edge clearance range 50% to 80%, both rod temperature difference and swing decrease as edge clearance decreases. The design basis for these results is as follows: rod pitch = 11.1 mm; rod diameter = 8.0 mm (smooth); internal support rods = 6; roughening of the heated section (1200-mm long) = 100%; and helium temperature = 302°C in, 585°C out (central fuel assembly).

Figure 2-36 shows rod diametral temperature difference and the associated swing as a function of the spacer solidity difference between the interior and the edge channels. Swing is defined as a condition in which the rod diametral temperature difference both increases and decreases within one spacer pitch and is typified by a sawtooth pattern, such as that shown in Fig. 2-35. The maximum value of swing is the difference between the

Fig. 2-35. Rod differential temperature profiles for 22 and 5 nodes (rod 28, channels 40 and 55/56)

Fig. 2-36. Maximum rod diametral temperature difference (rod 28, channels 40 and 55/56) versus spacer solidity difference (rod pitch = 11.1 mm; rod diameter = 8.0 mm; heated length = 1200 mm; interior channel spacer solidity = 0.195)

largest and the smallest value of rod diametral temperature difference within one spacer pitch and occurs in the maximum heat flux region. The following trends are apparent:

1. Rod diametral temperature difference
 - a. Decreases as spacer solidity difference increases.
 - b. Decreases as edge clearance decreases for a given spacer solidity difference.
 - c. Is maximum when the spacer solidity difference is zero.
2. Rod diametral temperature difference swing
 - a. Increases as spacer solidity difference increases.
 - b. Decreases as edge clearance increases for a given spacer solidity difference.
 - c. Is zero when the spacer solidity difference is zero.

It may be possible to select a design region which satisfies thermal-hydraulic, mechanical, and rod bowing requirements. In addition, duct gamma heating will be included in COBRA*GCFR runs because this results in an $\sim 15^{\circ}\text{C}$ reduction in maximum rod diametral temperature difference. Use of a roughened duct will also be studied since it may reduce rod temperature difference and yet permit large, desirable edge clearances.

2.2.2.1.3. PES Vent Connection

A piston and cylinder device containing a labyrinth seal has been proposed to provide directed flow of fission gas and bypass coolant into the

grid plate suction holes of the PES. The device would allow predictable flow into the monitor line even if the assembly were vertically displaced several centimeters from its rest position (e.g., because of a seismic disturbance). This basic design provides great flexibility for controlling flow characteristics by design parameter specification. The following parameters, indicated in Fig. 2-37, influence the pressure drop in this device:

- d = major passage diameter,
- δd = diametral clearance,
- l = step length,
- S = groove length,
- h = step height,
- r_s = radius of step corner,
- n = number of steps.

Calculations were performed to identify design parameter combinations expected to yield the required flow characteristics. (Flow tests will be conducted on three or four candidate designs to test the performance of the device and the validity of the design calculations.) The hydraulic resistance correlations of Ref. 2-50 were used for the calculations, and it was established that the pressure drop through the device should be about 15 kPa for a helium flow rate of 0.2 g/s at GCFR inlet conditions ($P = 10.5$ MPa, $T = 590$ K). As much diametral clearance as possible was desired to facilitate assembly, and a maximum pressure drop length of 45 mm resulted from overall space constraints. The parameters r_s and h were eliminated by choosing values that were reasonable from a fabrication standpoint and were such that moderate variations from their nominal values had little effect on the flow ($r_s = 0.01$ mm, $h = 1.0$ mm).

Figures 2-37 through 2-40 summarize the result of the calculations. The diametral clearance necessary to meet the 15-kPa pressure drop is plotted as a function of step height, and parametric curves are given for various groove lengths. Figures 2-37 and 2-38 give the results for a bore

Fig. 2-37. Results of calculations for device length of 45 mm and bore diameter of 6.0 mm

Fig. 2-38. Results of calculations for device length of 35 mm and bore diameter of 6.0 mm

Fig. 2-39. Results of calculations for device length of 45 mm and bore diameter of 7.0 mm

Fig. 2-40. Results of calculations for device length of 35 mm and bore diameter of 7.0 mm

diameter of 6 mm and device lengths of 4.5 and 3.5 mm, respectively; Figs. 2-39 and 2-40 are for a 7-mm bore diameter.

The parameters which are expected to have the greatest impact on the performance and assembly of the device are bore diameter, clearance, and device length. Hence, a test program should include a variation of these dimensions. In addition, it is important to verify the design basis for the device by performing a test in which these variables are fixed and the labyrinth dimensions varied. Table 2-19 presents the four design combinations which make up the flow test matrix.

2.2.2.2. Blanket Assembly.

2.2.2.2.1. Blanket Assembly Peak Cladding Temperature During a DBDA Transient

The steady-state rod bundle code SCRIMP was used to calculate and validate the DBDA radial blanket peak cladding temperature obtained with the transient system code FASTRAN. A full strip model (Fig. 2-41) was adapted to model the rod bundle thermal-hydraulic effects of a maximum-powered radial blanket assembly adjacent to the active core. A full strip model was used because radial power distribution in a blanket assembly decreases exponentially in the outward direction, and a full strip model is needed to represent adequate rod bundle effects such as flow redistribution.

The decay power in a radial blanket assembly consists of the decay power generated by the blanket assembly itself and the net gamma rays transported from the active core to the blanket assembly. The decay power distribution employed the latest algorithm developed in Ref. 2-51 and the gamma transport effect of Ref. 2-45. The peak-powered rod is the corner rod next to the active core (rod 56 in Fig. 2-41). Since FASTRAN can only model a single rod with a unit cell flow channel surrounding it, the transient study modeled the second highest-powered rod (rod 57 in Fig. 2-41). Because of the rod bundle heat transfer effects and the possession of a large edge

TABLE 2-19
FLOW TEST MATRIX

Dimension	Test 1	Test 2	Test 3	Test 4
Length (mm)	45	45	35	45
d (mm)	6	6	6	7
δd (mm)	0.27	0.27	0.245	0.241
S (mm)	0.65	0.5	0.65	0.65
ℓ (mm)	0.65	0.92	0.65	0.65
h (mm)	1	1	1	1

Fig. 2-41. Strip model of rod bundle

channel by the highest-powered rod, it is believed that unit cell modeling of the second highest-powered rod may generate the same hot spot cladding temperature that occurs on the peak-powered rod.

To mitigate the difference between a transient and a steady-state analysis, the unit cell heat flux of the second rod output from FASTRAN was adapted as the basis for the second rod power input to the SCRIMP code. The decay power radial distribution was then superimposed on the second rod power to obtain the rod power for the test of the rods in the strip model. Table 2-20 lists the resulting power distribution at the time of transient peak cladding temperature (1641 s) predicted by FASTRAN, and Fig. 2-42 gives the corresponding axial power distribution output from FASTRAN. Reference 2-31 gives the radial blanket rod cladding i.d. as 1.865 cm, the o.d. as 1.965 mm, and the lattice pitch as 2.105 cm. The rod bundle is wire wrapped, and the blanket fuel length is 203 cm.

The SCRIMP flow rate was determined by comparing its calculated core pressure drop (from lower to upper plenum) with the FASTRAN value of 5.103 kPa using parametric calculations. Additional computations were performed

1. To obtain the hot spot temperature using the latest hot spot factors for low-flow conditions (Ref. 2-49), i.e.,

$$\begin{aligned}f_{\text{channel}} &= 1.332, \\f_{\text{film}} &= 1.477, \\f_{\text{clad}} &= 1.142.\end{aligned}$$

2. To exclude the circumferential conduction effect.
3. To exclude the radiation effect.
4. To exclude circumferential conduction and radiation effects.

TABLE 2-20
LINEAR POWER DISTRIBUTION IN THE MAXIMUM-POWERED
RADIAL BLANKET ASSEMBLY

	Linear Power (W/cm)			
	Full Power		1641 s of DBDA	
	Maximum	Average	Maximum	Average
Fuel rod(a)				
56	452.28	218.39	11.094	5.748
57	365.07	176.28	6.736	3.490
58	308.75	149.08	5.223	2.706
59	259.81	125.45	4.350	2.254
60	226.67	109.45	3.846	1.993
61	195.09	94.20	3.362	1.742
62	171.47	82.80	3.013	1.561
63	151.79	73.29	2.704	1.401
64	133.41	64.42	2.704	1.401
72	415.29	200.53	11.547	5.983
71	365.07	176.28	6.736	3.490
70	308.75	149.08	5.223	2.706
69	259.81	125.45	4.350	2.254
68	226.67	109.45	3.846	1.993
67	195.09	94.20	3.362	1.742
66	171.47	82.80	3.013	1.561
65	151.79	73.29	3.154	1.634
Duct wall surface(a)				
52	28.17	13.60	2.531	1.311
53	11.78	5.69	1.058	0.548

(a) See Fig. 2-41.

Fig. 2-42. Normalized axial power distribution for radial blanket rod at 1641 s for DBDA = 1905 mm²

The nominal maximum cladding surface temperature at 1641 s of DBDA was 1046°C at the inside surface (away from the active core) of the peak rod at ~71% of the rod length from the bundle inlet. Figure 2-43 shows the axial temperature profiles of cladding surface 3 (the hot spot surface) and coolant subchannel 3, and Fig. 2-44 shows the radial temperature distribution in the neighborhood of the hot spot. Table 2-21 compares the maximum cladding surface temperatures of all the cases studied.

The calculated hot spot temperature is 1424°C, which is 124°C higher than the ANS PC-5 faulted limit of 1300°C. The calculated temperature is designed related and can thus, be changed by design revisions. The high cladding hot spot temperature is a consequence of the small rod-to-rod gap (~0.14) allowed in the blanket design. A new radial blanket assembly design which concentrates on enlarging the rod-to-rod gap to ensure that the hot spot temperature is below the PC-5 faulted limit will be examined. The following conclusions were drawn from this analysis:

1. The nominal maximum cladding surface temperature of 1046°C compares favorably with the FASTRAN value of 1075°C, demonstrating that FASTRAN unit cell modeling is adequate for blanket rod analysis.
2. Table 2-21 indicates that the maximum cladding surface temperature may increase by approximately 28°C if circumferential conduction is excluded and by approximately 167°C if radiation is excluded. This temperature could be increased by approximately 221°C if both circumferential conduction and radiation are excluded.
3. Figure 2-44 shows that the average cladding surface temperature of peak-powered rod (rod 56) is only about 20°C higher than that of the second rod next to it. This further warrants the adequacy of unit cell modeling.

Fig. 2-43. Nominal axial temperature profiles for cladding surface 3 and coolant subchannel 3

Fig. 2-44. Radial temperature distribution ($^{\circ}\text{C}$) at axial location of maximum cladding temperature

TABLE 2-21
MAXIMUM CLADDING TEMPERATURES AND FLOW RATES

Nominal Case	Maximum Cladding Temperature (°C)	Flow Rate (kg/s)
Including circumferential conduction and radiation	1046(a)	Strip model = 0.00469 Unit cell = 0.00012(b)
Excluding circumferential conduction	1074	Strip model = 0.00469 Unit cell = 0.00012(b)
Excluding radiation	1213	Strip model = 0.00469 Unit cell = 0.00012(b)
Excluding circumferential conduction and radiation	1267	Strip model = 0.00469 Unit cell = 0.00012(b)

(a) FASTRAN - 1075°C.

(b) The flow rate of a unit cell is equal to twice the flow rate of an interior channel.

4. The noticeable coolant temperature difference among subchannels 1, 2, and 3 is partly due to SCRIMP modeling of no turbulent mixing and no enthalpy mixing within an axial section, which is conservative.

2.2.3. Core Configuration and Performance Analysis

More detailed information has been obtained on power peak melting owing to burnup effects and variable control rod positions, and it has thus become necessary to either provide extra heat transfer surfaces in the core or accept a lower core outlet temperature. Sufficient information on cladding temperatures at BOC and EOC is available for the demonstration plant core (169 assemblies), and initial conditions can be chosen so that maximum allowable cladding temperatures will not be exceeded at any time. This information is not available for the commercial plant (469 assemblies), but an approximate basis for the commercial plant design has been developed from the demonstration plant information. BOC conditions can be fairly well estimated for the commercial plant, but EOC conditions require further study.

The decision to concentrate on a three-loop instead of a two-loop demonstration design reduces the emphasis on strictly prototypical demonstration plant components. If circulator power per loop is maintained at a given value, it is possible to design the core for different power levels by slight revisions of the rod lattice spacing, with corresponding different core outlet temperatures. Since a demonstration plant with prototypical assemblies would have an outlet temperature of only 530°C (with the updated peaking factors), it might be desirable to increase the outlet temperature by dropping the output power level. Calculations show that the outlet temperature can be increased to 544°C by dropping the electric power generated from 377 to 333 MW and reducing the rod-to-rod clearance from 3.5 to 3.0 mm.

2.2.4. Core Structural Analysis

2.2.4.1. Reactivity Insertion During Loss of Flow Accident. Structural bowing as a source of negative insertion during a loss of flow accident [high power-to-flow ratios (P/F)] has been analyzed. The first step of the study investigated why the NUBOW-2D (Ref. 2-52) code predicted a much larger reactivity insertion than calculations using a simply supported indeterminant beam with two lateral restraints (load pads) to compute an average outward deflection of the active core region. Using the reactivity results of Ref. 2-53 and a simply supported indeterminant beam method, a reactivity insertion of $-\$0.109$ was computed for an outlet temperature gradient of $0.57^{\circ}\text{C}/\text{mm}$. A similar NUBOW-2D model with a seventh-row outlet gradient of $0.57^{\circ}\text{C}/\text{mm}$ ($P/F = 1.38$) gave a reactivity insertion of $-\$0.20$. The difference was suspected to be in the outward motion of the load pads predicted by NUBOW.

Figures 2-45 and 2-46 show that the load pads move outward from a vertical line passing through the assembly connection in the grid plate (0.0 axial distance). The figures were drawn by adding the following to the predicted centerline bow: (1) left and right radial expansion due to a temperature increase from 20°C and (2) load pad thickness (resulting in triangular spikes in the curves). The duct profiles were separated by the hot gap distance. The resulting profiles represent the outer shape of each duct collapsed around the initial cold flat-to-flat dimension. The interduct distances are full scale. Careful adjustment of the load pad clearances and the restraint ring clearances made it possible to align the load pads vertically with the grid plate connection. The predicted reactivity insertion with vertically aligned load pads was $-\$0.092$, which is in reasonably close agreement with the value obtained in the simply supported beam method, considering the large differences in the techniques.

The second step of the study explored the effect of high P/F which would occur in a loss of flow accident. Two cases were considered: (1) core former restraint ring temperature held constant at inlet temperature

Fig. 2-45. Bowed profiles collapsed around distorted centerline ($P/F=1.5$), rows 2 through 6

Fig. 2-46. Bowed profiles collapsed around distorted centerline ($E/F=1.5$), rows 7 through 11

values and (2) restraint ring temperature raised in proportion to P/F. In both cases, the assembly wall temperatures and radial gradients were raised proportionally with P/F level.

Figure 2-47 shows the reactivity insertion for the start-up transient ($P/F = 0 \rightarrow 1$) and for accident conditions ($P/F = 1 \rightarrow 3+$). Although there is a slight positive insertion at $P/F = 1$, the trend is in the direction of a negative insertion. Further refinement of the restraint ring design could move this point in any direction. For the case of constant restraint ring temperature, there is a tendency to limit the amount of insertion at a high P/F because at a point near $P/F = 1.5$, the entire core becomes locked at both load pads (Figs. 2-45, 2-46). Increasing the gradients can only cause further assembly distortion rather than load pad movement, thus limiting the amount of insertion. This is not the case when the restraint ring is allowed to follow the accident. In this case, the negative reactivity insertion continues indefinitely.

The following conclusions have been reached:

1. A large negative insertion can be expected to accompany a rise to a large P/F. The amount of insertion is highly dependent upon the temperature of the core restraint ring.
2. Reactivity changes in a restrained core differ from those predicted by simple beam theory with double restraints due to outward movement of the load pads (restraints) and subsequent enlarging of the core.
3. Changes in the temperature fields can radically influence the results. Thus, it is important to repeat this study as soon as the fuel assembly design is finalized and accurate temperature and neutron flux fields are available. The assembly pitch and restraint ring can then be designed more accurately.

Fig. 2-47. Reactivity insertion due to structural bowing

2.3. FUEL ASSEMBLY DESIGN AND ANALYSIS

The fuel assembly inlet nozzle is located within and directly above the core grid plate. It contains shielding to protect the grid plate from excessive neutron streaming and an assembly fission product trap to delay the evacuating fuel rod fission gases and is equipped with an inside venturi-shaped pneumatic balancing device which minimizes assembly uplift forces. Two ways of designing the inlet nozzle subassembly were examined (Fig. 2-48).

Labyrinth sealed vent connections between the assembly inlet nozzles and the PES matrix plate and located on top of the grid plate were investigated. The labyrinth concept was developed for accomplishing the vent connection while accommodating a possible assembly uplift due to ratcheting or a seismic event. To test this concept, a mock-up of the new labyrinth seal was made. The in-core connection and test fixture are shown in Figs. 2-49 and 2-50, respectively.

Alternate candidate spacer grid concepts were explored. The current reference electrodischarge machined (EDM) spacer grids (Fig. 2-51) are costly to manufacture and have definite material swelling problems in a GCFR environment. The alternative design concepts were therefore primarily compliant spacer grids of the EDM and flat strip egg-crated types (Figs. 2-52 through 2-54). The most promising concept will be determined based on thermohydraulic and swelling characteristics.

Various design studies were performed to investigate the construction of the fuel bundle skeleton assembly, which consists of six axial members which support the spacer grids. Simplicity, manufacturing cost, and ease of assembly were emphasized. The preferred design is a bundle skeleton in which the axial members connect the grid manifold plate and all the spacer grids with individual rods. Each rod is the length of the span between spacers and is male threaded at one end and female threaded at the other. The rods are threaded together to firmly capture each spacer grid in

SECTION D-D

SECTION B-B

SECTION C-C

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Fig. 2-48. Inlet region layout for fuel and blanket assembly

2-129

SECTION A-A

Fig. 2-49. PES vent connection layout for upflow core

2-130

-001

1	5	PLUG	55/SWAGELOR-6MO-P	
	8	1/8" MALE CONNECTOR TO NPT	55/SWAGELOR-400-1-2	
	7	1/2"-20 UNC-2A X 1.10 LG SCM SCR	5531600	
	6	#8-56 UNF-2A X .787 LG SOC SET SCR, HALF DOG	5531600	
	5	O-RING	VITON PARKER NO.2-151	
	4	TOPOC687 001, 002, 008	LABYRINTH VALVE STEM	
	3	TD20C664	FLANGE, BOTTOM	
	2	TD20C663	FLANGE, TOP	
1	TD20C666	HOUSING		
ITEM		PART NO	DESCRIPTION	MATL V4TL SPEC
REQD/ASSEMBLY				
PARTS LIST				

Fig. 2-50. Fixture assembly for PES labyrinth test

2-131

Fig. 2-51. EDM spacer grid

DIMENSIONS IN MM

DETAIL E

SCALE: $5/1$

VIEW F-F (ROTATED 30° CCW)

SCALE: $5/1$

Fig. 2-52. EDM grid spacer with springs

Fig. 2-53. EDM grid spacer with spring module

Fig. 2-54. Rhombic grid spacer, spring/button concept

between. The tubes are roughened in the fuel zone of the bundle and smooth in the remaining spans (Fig. 2-55).

2.4. SHUTDOWN ASSEMBLY DESIGN AND ANALYSIS

There was no activity on this subtask during this quarter.

2.5. BLANKET ASSEMBLY DESIGN AND ANALYSIS

There was no activity on this subtask during this quarter.

2.6. CONTROL ASSEMBLY DESIGN AND ANALYSIS

Concepts for providing the control assembly exit region with the required neutron shielding are being investigated.

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Fig. 2-55. Support rod, locking concept

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3.0 PROJECT SUPPORT

3.1. QUALITY ASSURANCE

There was no activity on this subtask this quarter.

3.2. PLANT SIMULATION

A two-loop model (Fig. 3-1) is being used to develop the load control portion of the plant control system. The three primary coolant (helium) main loops are represented by two model loops, A and B, with A representing one plant loop and B representing two plant loops. The two secondary coolant loops (water/steam) are represented by two model loops, also A and B. Only components needed to represent the plant under normal operating conditions are included at this time. The thermodynamics of the deaerator are not modeled, but the boundary conditions are included.

Figure 3-2 shows the control system model currently being studied. The controllers have been modeled as proportional integral derivative controllers, and the ranges selected for the sensors are noted in Fig. 3-2. These ranges were selected to cover known steady-state variations over the normal load range and expected transient variations. At present, only the 50% to 100% load range has been analyzed. The bias and range of all sensors will be subject to change as further dynamic studies are performed. The dynamics of the main steam temperature sensor and module outlet temperature sensors have been modeled as first-order lags with a 10-s time constant. The dynamics of the remaining sensors have not been considered.

The two-loop model has been used to develop the steam generator module outlet temperature controller. The circulator speed demand is composed of two components. One is designed to maintain helium flow through each steam generator in fixed proportion to feedwater flow through that steam generator

Fig. 3-1. Model of two-loop equivalent GCFE plant

Fig. 3-2. Model of control system

for the normal plant load range. The other is based on a set point computed to be the average of the steam temperatures measured at the outlet of the three steam generator modules. This average temperature was compared with the actual outlet temperature in a particular loop to obtain the temperature error signal for that particular loop. The feedwater flow to helium flow characterizer curve was derived from steady-state calculations, and the circulator motor and motor speed controller were represented by a first-order lag, since complete models of these components have not been developed yet.

The module outlet temperature controller was not included in the single-loop simulation because testing of this controller required a multimodule simulation to find the average module temperature set point. Since the set point is a computed value rather than an adjustable variable, a method other than set point variation was chosen to demonstrate the response of the controllers. A 10% step reduction in the area of the module feedwater control valve in the A loop produced the responses shown in Fig. 3-3. The primary variable being considered is the temperature of the steam at the outlet of steam generators A and B. The response of this variable is very slow compared with the response of main steam temperature or main steam pressure. This is appropriate for this control function.

The full load control system gain settings were used for this simulation. Addition of the module outlet temperature controller slightly reduced system stability, but a small reduction in regulator rod drive rate returned system stability to an acceptable level.

The disturbances which occurred during the transient at approximately 510 and 830 seconds were the result of neutron flux adjustments made by the regulator rods. Figure 3-3 shows the low thermal capacity of the GCFR core and the almost instantaneous effect the increase in neutron flux has on main steam temperature. This occurs because the helium passing through the core comes in direct contact with the fuel rods and is rapidly heated by the increase in reactor power. The heated helium then passes from the reactor

Fig. 3-3. Response of GCFR to 10% step reduction in area of module feedwater control valve in loop A (sheet 1 of 2)

Fig. 3-3. Response of GCFR to 10% step reduction in area of module feedwater control valve in loop A
(sheet 2 of 2)

core directly to the superheater section of the steam generators, where it produces an increase in main steam temperature.

3.3. SYSTEMS ENGINEERING

3.3.1. Systems Integration

The Plant Technical Description (Ref. 3-1) has replaced the Baseline Data Book (Ref. 3-2) as the top-level design document for the demonstration plant. The first issue of the Plant Technical Description anticipated a change to a two-loop plant and will be updated to reflect the decision to stay with a three-loop plant following resolution of major plant parameters.

The safety and licensing requirements governing the PES design are being reviewed.

3.3.2. System Design (NSSS)

3.3.2.1. Primary Coolant System. During the last quarter, a set of design parameters was formulated and components sized for a 3600-MW(t) large GCFR. This quarter, identical components were used to derive design parameters and full-load design conditions for a proposed two-loop demonstration plant.

The circulator drive motor power was established at 30,000 hp (22.4 MW), which includes a 30% margin. This margin was tentatively chosen so that the circulator could accommodate a 7% increase in flow plus a 6% increase in the primary loop system resistance characteristic.

Methods for measuring reactor power during operation were evaluated to satisfy the intent of Regulator Guide 1.49, which states that safety analysis in support of a nuclear power plant license application should be made at 1.02 times the licensed power to allow for instrument errors in measuring power level.

Three methods were considered for measuring reactor power: (1) a heat balance on the core, (2) a gas-side heat balance on the steam generator, and (3) a steam-side heat balance on the steam generator. The steam-side heat balance on the steam generator could give the most accurate determination of reactor power, and special calibration of the feedwater flow measurement would be required to produce an uncertainty within $\pm 2\%$. A less sophisticated measurement could easily result in 5% uncertainty in reactor power, but a value of 2% is being used for the interim period.

A rationale was developed for determining the nominal operating conditions for the GCFR power plant. Given that the core sets the limiting conditions of operation and according to Regulating Guide 1.49 the licensed (i.e., nominal) power should be a factor of 1.02 less than the maximum design power, the nominal power for the 3600-MW(t) plant would be 3529 MW(t). In addition, if it is assumed that the technical specifications include a limiting condition of operation which is related to maximum cladding temperature, instrument errors affecting variables which influence the prediction of maximum cladding temperature would further downgrade the nominal operating conditions. This would result in downgrading the core outlet gas temperature by approximately 5.6 K. Thus, the nominal operating conditions for the proposed 3600-MW(t) plant (including the latest core peaking factors) would be as follows:

	<u>Core Design</u>	<u>Nominal Operation</u>
Reactor power [MW(t))]	3600	3529
Core outlet gas temperature (K)	819	813

There is a serious problem in maintaining a core outlet temperature high enough to generate the 783-K steam previously specified. Increasing circulator pumping power or redesigning the core are two possible solutions to this problem.

The estimated heat loss to the CACS cooling water system at 100% power is 3.1 MW(t) per CACS loop. This heat loss could be reduced by lowering the CAHE in its cavity. However, this would reduce the natural convection cooling capacity during accident conditions when the CACS is required.

A thermal time constant for a typical GCFR upper plenum radiation shield has been established using a two-loop upflow core in which the primary coolant flow is through vertical rectangular slots cut through the radiation shield. There is a thermal time constant of approximately 825 s for this or similar types of shields. This lengthy time constant indicates that for normal power operation, the radiation shields are of little value as thermal delay beds, and are of even less value during RHR, when there is less flow and helium heat transfer.

The GCFR demonstration plant has been changed from a two-loop to a three-loop plant. Hence, various strategies for going from a demonstration plant to a commercial plant are being evaluated. All the plant design parameters are being reconsidered, including plant power level.

One strategy assumes a three-loop, approximately 400-MW(e) demonstration plant which has fuel assemblies and operating conditions similar to a 1200-MW(e) large plant. The steam generators and circulators would be different for each plant. However, the option to commercialize the demonstration plant technology has been preserved by designing a six-loop "demonstration plant" of 800 to 1000 MW(e). There is a significant stretch capability inherent in the change from three to six loops using demonstration components owing to the automatic increases in reactor outlet temperature and to realistic use of margins. Slight increases in circulator power are effective in increasing reactor outlet temperature and alleviating potential conflicts between core performance and steam cycle performance.

A second strategy uses certain parameters from the large plant study plus a smaller circulator power based on an existing electric motor design.

This approach would limit the power level and operating conditions which could be achieved in commercializing the demonstration plant technology.

3.3.2.2. Residual Heat Removal. Alternative shutdown cooling water systems are being evaluated by an architect/engineer. In particular, a three-loop system is being studied. Preliminary information describing a boiling shutdown cooling water system was prepared for the two-loop demonstration plant. Design requirements and objectives, operating modes, alternative design approaches, and the advantages/disadvantages of the alternatives were studied.

Two studies on the drum boiler shutdown cooling system have been completed. The first investigated static stability for operation in the pressure range 4.82 to 13.79 MPa with a 15% feedwater flow. A drum boiler shutdown cooling system would be stable in this operating range. The second study investigated operation at atmospheric pressure and considered two cases: (1) refueling 24 h after shutdown with 5% feedwater flow and (2) pressurized cooldown during peak heat duty operation. The shutdown cooling system was stable for the first case. For the second case, the feedwater flow required to provide adequate static stability resulted in a pressure drop about six times greater than the available natural convection head.

A boiling CACS was developed which consisted of a CAHE and auxiliary loop cooler (ALC) which are nearly the same size as those for the all-liquid design. The boiling system includes a drum separator which is 6.1 m tall and 2.4 m in diameter for each loop. This system is compatible with both the two- and the six-loop designs and has the following advantages: (1) inherent stability and self-regulation; (2) diversity; (3) simpler CAHE design; (4) potential elimination of some controls and components; and (5) potential for short-term cooling on stored water only. In addition, the possibility of cooling using the "fire water" system at 310 kPa as a heat sink was investigated, and it was found to be stable and adequate for providing long-term cooling in the event that the air-cooled condenser was not able to operate. However, this system was not adopted.

The influence of natural convection in the isolated CACS loops during normal power operation was examined. A simple, straight duct geometry was assumed (this geometry is an alternative to the reference configuration). For this geometry, leakage flow past the CACS isolation valve will not suppress natural convection of hot helium into the CACS cross duct.

A preliminary evaluation of the steam generator's capability to self-isolate during pressurized natural circulation cooldown on the CACS has been performed. This evaluation has shown that the self-isolation capability is highly design dependent, and the bottom exhaust steam generator probably cannot self-isolate. It is also probable that the top exhaust steam generator can be designed for self-isolation. If self-isolation is not achieved and the main loop valves do not close, it would be impractical to resize the CACS for adequate RHR. Furthermore, for some designs, it may be necessary to connect all steam generators to a CAHE loop to assure isolation, even though the steam generator may meet the current criteria for self-isolation. Self-isolation during the initial part of the cooldown transient has not been evaluated.

The SACSAP code* has been updated to include the following options:

1. Heat exchanger sizing and performance with forced air flow in the ALC.
2. Heat exchanger sizing with natural draft in the ALC (nonoperating fan installed in ALC).
3. Heat exchanger sizing with natural draft in the ALC (no fan).
4. Heat exchanger performance with combined natural and forced draft in the ALC.

*SACSAP sizes the entire CACS and calculates its performance. The code is still under development.

5. Heat exchanger performance with natural draft in the ALC (non-operating fan installed in ALC).
6. Heat exchanger sizing with natural draft in the ALC (no fan).

Each option can be used with natural or forced flow in either the helium or water loops.

3.3.2.3. Steady-State Plant Performance. The heat balance and steam cycle conditions for the six-loop 3600-MW(t) large plant were completed, and conditions for the two-loop demonstration plant were generated assuming the same prototypical components. If an 1800-rpm tandem compound turbine is used for the large plant, the plant efficiency is 36.0%; if a 3600-rpm tandem compound turbine is used for the demonstration plant, the plant efficiency is 35.1%. Tables 3-1 through 3-3 present typical performance data (these data do not include adjustments for a recent change in core radial peaking factors).

Development work was performed on the SCAP code (Ref. 3-3), a plant steady-state performance code, to make it more versatile and applicable to GCFR needs. Work was also done on NSSPERF (Ref. 3-4), a primary coolant system performance code.

3.3.2.4. Parametric Design Models. A detailed steam system model is being developed for the SOFAST code. The design and cost calculations for the steam and feedwater systems were programmed and tested, and test cases were run using main steam temperature, throttle pressure, main steam piping ΔP and feedwater piping ΔP as variables. Because SOFAST will be used in examining a wide range of thermal power [from 50 to 3600 MW(t)], several improvements were made:

1. Steam generator subroutines were reworked to allow design of steam generators over a larger range of thermal ratings. Tests have been run from 15 to 600 MW(t).

TABLE 3-1
COMPARISON OF KEY PARAMETERS FOR LARGE PLANT AND
DEMONSTRATION PLANT

	Demonstration Plant	Large Plant
Fixed parameters		
Plant rating [MW(t)]	1122	3600
Number of main loops	2	6
Number of CACS loops	3	3
Major parameters		
Maximum cladding temperature (°C)	750	750
Core outlet temperature ^(a) (°C)	544	557
Steam generator helium inlet temperature (°C)	538	553
Steam outlet pressure at steam generator (MPa)	13.2	13.2
Steam outlet temperature at steam generator (°C)	510	510
Feedwater temperature (°C)	184	191
Core inlet temperature (°C)	302	302
Circulator power margin (%)	30	30
Helium pressure ^(b)		
Performance analysis (MPa)	10.5	10.5
Structural design (MPa)	11.0	11.0
Resultant performance parameters		
Circulator motor shaft output power, including margin (MW)	22.4	22.4
Plant electrical rating (MW)	393	1297
Plant efficiency (%)	35.1	36.0
Reactor pressure drop (kPa)	179.3	179.3
Total helium loop pressure drop (kPa)	242.7	240.0
Core helium flow rate ^(c) (kg/s)	890.9	2707.9

(a) Mixed mean temperature of core + blanket + allocated core bypass.

(b) Circulator discharge.

(c) Includes core + blanket + allocated core bypass flows.

TABLE 3-2
SUMMARY OF PRIMARY LOOP PRESSURE LOSSES (kPa)
FOR TWO-LOOP DEMONSTRATION PLANT

Duct losses from circulator discharge to core inlet plenum	4.1
Core inlet plenum shielding ΔP	2.1
Overall reactor ΔP	179.3
Core outlet plenum shielding ΔP	1.4
Duct losses from core outlet plenum to exit plane of hot cross duct	0.7
Overall steam generator ΔP from hot cross duct exit plane to duct flange at steam generator outlet	42.1
Duct losses from steam generator outlet flange to circulation inlet flange assuming a horizontal circulator	1.4
Allowance for anticipated ΔP values that become apparent during conceptual design	11.7
Total	242.8

TABLE 3-3
SYSTEM HEAT BALANCES(a)

	MW(t)(a)	
Primary system heat balance		
Energy added by core, including blanket, reflector, and shielding	+1121.5	
Energy added by helium circulators	<u>+34.0</u>	
Total energy input	+1155.5	
Energy lost to CACS cooling water system	-9.4	
Energy lost to PCRV liner cooling water system	<u>-5.3</u>	
Total energy lost	-14.7	
Net energy transferred to steam	<u>-1140.8</u>	
Balance	0.0	
Helium circulator system heat balance(b) (per loop)	Without Margin	With Margin
Power to circulator motor speed control and power supply unit	+18.9	+24.7
Power loss of speed control and power supply unit	<u>-0.5</u>	<u>-0.7</u>
Total power to circulator motor	+18.4	+24.0
Overall power loss in motor, including bearings, windage, heating, thrust bearing, etc.	<u>-1.2</u>	<u>-1.6</u>
Motor shaft output power	+17.2	+22.4
Circulator bearing losses	-0.2	-0.3
Circulator power added to helium	<u>-17.0</u>	<u>-22.1</u>
Balance	0.0	0.0

(a) Input is positive.

(b) Circulator speeds are approximately 2800 and 3000 rpm for the without margin and with margin cases, respectively.

2. The velocity head in the ducting can be the same from case to case.
3. The CACS cavity diameter, which was formerly assumed to be constant, is now a function of auxiliary loop heat exchanger area.

SOFAST was run over the 50 to 400 MW(t) range, and the cost results agreed within 3% of an independent estimate, confirming the code.

3.3.2.5. Advanced Concepts. Several RHR improvement concepts are being considered: (1) containment pressurization to enhance natural convection cooling, (2) natural convection of steam generators (water/steam side) for shutdown cooling system operation, and (3) loop arrangements for natural convection on the primary side.

3.3.2.6. Large Plant Scaling Studies. The scope of this subtask is as follows:

1. Configuration studies will resolve open configuration issues or improve on an existing scheme.
 - a. Symmetric/asymmetric PCR/V layout. Symmetric and asymmetric preliminary layouts of the PCR/V will be prepared in sufficient detail to support BOP building layout work and a PCR/V cost differential estimate.
 - b. Building layout. Using the PCR/V layouts, overall building layouts will be prepared in sufficient detail to determine a cost differential estimate.
 - c. PCR/V/building evaluation. Evaluation criteria will be assembled for deciding between the asymmetric and symmetric PCR/V/building designs.

- d. Refueling. Means of reducing the commercial plant refueling time will be examined using the reference refueling concept.
 - e. Systems and plant design. Design requirements, configuration, and performance information will be developed for the primary coolant system, overall plant, shutdown cooling system, and CACS.
2. System and component design studies will relate demonstration plant component designs to the large plant, and the conceptual design will be completed.
- a. NSSS component sizing and design. Demonstration designs will be scaled up.
 - b. PCR/V/closures layout and sizing. Layout drawings of the PCR/V and the major closures will be generated.
 - c. Auxiliary circulator design. Circulator impellers will be sized for the large plant and compared with the demonstration plant. (Other CACS component designs are expected to be nearly the same as those for the demonstration plant.)
 - d. Control systems/transients. Design requirements, and expected performance of the operating plant system and plant protection system will be developed using representative transients.
 - e. BOP design. Sizing layouts and/or specifications will be developed for the containment, confinement, and reactor service buildings; reactor plant cooling water; radioactive waste; service water; and electrical systems.

3. Documentation. A draft conceptual design report will be prepared which describes the large plant and explains the rational basis for parameter selection and design configuration decisions.

The symmetric/asymmetric PCRV studies have been started. The symmetric PCRV arrangement has the steam generator and CACS cavities distributed as evenly as possible around the core cavity, whereas the asymmetric arrangement puts all the CACS cavities on one side with the balance of the PCRV periphery filled with steam generator cavities. The initial results indicate that an asymmetric PCRV with a core offset from the PCRV center is larger than a symmetric PCRV. Reducing the core offset reduces the asymmetric PCRV diameter. The smallest diameter is obtained when the offset is zero, in which case the asymmetric PCRV diameter equals the symmetric (based on the use of six main steam generators and three CACS loops).

GCFR startup/shutdown durations and maintenance/in-service inspection requirements have been reviewed with regard to the annual refueling outage duration goal of 21 days. The review was based upon HTGR precedence and Fort St. Vrain experience. The GCFR assumed startup time from refueling of 36 h appears reasonable, and the assumed shutdown time to refueling could be reduced from 72 to 48 h. A comparison of HTGR and GCFR estimated total commercial plant refueling outage times (with adjustable orifices and improved time to shutdown) gave requirements of ~22-1/2 days for the HTGR and ~28 to 29 days for the GCFR. However, the GCFR refueling procedure has not yet been finalized, and potential improvements for the GCFR may be possible.

3.3.3. System Design

3.3.3.1. Demonstration Plant. The feasibility of starting the shutdown cooling system in 30 s or less was evaluated. Startup in 30 s is feasible if the time required to open and close the isolation valves involved is reduced from 20 to 10 s. This change is within the range of the existing technology.

Pumping data were obtained, and the pressure drop through an inoperative pump was determined. This information was required for an analysis of the CACS natural convection mode with cooling water flow to the CAHE through the (nonoperating) pump.

In-service inspection of core cavity components was examined and the following method was proposed: viewing augmented with material surveillance for components exposed to elevated temperatures and/or radiation effects.

3.3.3.2. Commercial Plant. A study was made of a proposed piping arrangement for the six-loop plant. Based on the pipe sizes initially estimated, the total pressure drop from the steam generator outlet to the turbine admission was about 0.86 MPa. With reduced flows due to lower feed-water temperature the total pressure drop decreased to about 0.73 MPa. Since the allowable pressure drop has been increased to 0.69 MPa, it was concluded that the piping arrangement is acceptable.

3.3.4. Materials Engineering.

3.3.4.1. Irradiation Effects Data on Candidate GCFR Out-of-Core Structural Materials. Updated irradiation effects data requirements for design of out-of-core structural components of the two-loop upflow GCFR demonstration plant were input to the national program. Table 3-4 summarizes the updated design conditions.

An extensive literature search is under way to compile irradiation effects data on material being considered for GCFR out-of-core structural components. Extensive irradiation data relating to tensile creep rupture and fatigue properties, fracture toughness, fatigue crack growth, and swelling are being analyzed. The structural materials being studied for the GCFR include 316 and 304 stainless steel, HT-9 (12 Cr steel), 9 Cr -1 Mo, Inconel 718, Hastelloy X, and their weldments. Emphasis has been placed on understanding the effects of the irradiation spectrum on material properties.

TABLE 3-4
CURRENT (UPFLOW) TWO-LOOP TEMPERATURE AND RADIATION EXPOSURE CATEGORIES
FOR GCFE DEMONSTRATION PLANT STRUCTURAL COMPONENTS^(a)

	Applicable Component	Maximum Metal Temperature (°C)	Maximum Total Fluence (nvt)
Primary coolant system boundary	PCR _V (concrete, liner, etc.), reactor closure	Very low (~70°C)	Very low (~1 x 10 ¹⁹)
Upper reactor plenum	Upper radial shielding assembly attachments, fuel unlocking mechanism penetration ports, upper axial shield and attachments	566°C	<1 x 10 ²¹
Core	Thermal barrier components	566°C	1 x 10 ¹⁹
	Exit shielding ^(b)	579°C	5 x 10 ²²
Core support system	Reactor grid plates, core support cylinder	450°C	~1 x 10 ²²
	Core barrel	566°C	1 x 10 ²¹
Lower reactor plenum	Lower plenum shield assemblies and attachments	371°C	1 x 10 ²²
	Thermal barrier components	302°C	1 x 10 ¹⁹

(a) Reference metallic materials: 316 stainless steel, carbon steel, Hastelloy X, 316 stainless steel weldment. Alternate metallic materials: 304 stainless steel, HT-9 (12 Cr steel) and/or 9 Cr - 1 Mo, Inconel 718.

(b) Part of fuel assembly.

Consideration is being given to a lifetime-limiting criterion other than one based on residual total tensile elongation which can predict the material performance more accurately and thus impact plant design (such as fluence, temperature, size, and other economic constraints).

3.3.4.2. EBR-II Structural Materials Irradiation Test. Tests to obtain near-term tensile data on irradiated high-temperature 316 stainless steel (base and weld metals) are under way to establish the fluence limits for the design of GCFR out-of-core structural components at HEDL. Table 3-5 shows the test matrix. The rods have been inserted in the EBR-II reactor, and rods at fluences ($E > 0.1$ MeV) of 0.5 and 1×10^{22} nvt have been removed.

3.4. SAFETY AND RELIABILITY

The purpose of this task is to investigate and quantify GCFR safety characteristics. In accordance with the GCFR safety program plan, the safety task has been organized into five subtasks:

1. The overall safety program definition and integration task defines and organizes the activities in the individual tasks, provides for coordination of the national and international GCFR safety programs, and ensures that all important aspects of GCFR safety are studied with a balanced set of priorities.
2. The probabilistic technology task is aimed at defining event sequences leading to loss of coolable core geometry and quantifying their probability of occurrence.
3. The core accident technology task focuses on the response of the core to events involving loss of coolable core geometry. The objective of this task is to quantify core energy release and fuel vapor generation that may occur before a permanently subcritical configuration is established.

TABLE 3-5
EBR-II IRRADIATION EXPERIMENT(a)

Temperature (°C)	Fluence (n/cm ² x 10 ²²)	316(b) Base Metal Specimens	316(c) Weld Metal Specimens
550	0.5	6	6
	1	6	6
	2	6	6
650	0.5	6	6
	1	6	6
	2	6	6

(a) Three B-7 rods in row 7.

(b) Three heats, two replicates.

(c) Three welds (GTAW, SMAW, SAN) in 25.4-mm plate with 316
fillter, two replicates.

4. The primary containment technology task focuses on the response of the PCRV to the conditions established at permanent core sub-criticality by the core accident technology task. The objective of this task is to quantify the consequence mitigation characteristics provided by the PCRV, principally with respect to molten fuel containment, energy release containment, and fuel aerosol containment, to ensure protection of secondary containment integrity. Furthermore, this task will establish a mechanistic basis for demonstrating the conservatism of the secondary containment site suitability source term.
5. The secondary containment technology task focuses on the secondary containment as a barrier to the release of radioactivity. The objective of this task is to quantify the response of the secondary containment to the conditions established by the primary containment technology task to ensure that secondary containment integrity is maintained for a significant period of time and that the radiological consequences are limited to acceptable levels for extremely low-probability events. This task will also ensure that the radiological consequences of a site suitability source term release to the containment are within 10CFR100 guidelines.

Work is currently in progress toward attaining the objectives identified above. Logical probabilistic methods are being employed to determine the probabilities associated with various accident initiation and progression sequences and to identify potential design modifications that would help reduce risks. A reliability data bank is maintained and continuously updated to support these analyses. A methodology for integrating reliability considerations into the GCFR engineering effort at the system, subsystem, and component levels has been developed, with the objective of determining the optimal use of reliability engineering methods in the GCFR.

Core accident consequence analyses are concentrating on the loss of shutdown cooling (LOSC) accident sequence, which has been identified as a

dominant event sequence that could lead to core melting. Recriticality in this accident sequence is the phenomenon that could most significantly influence consequences. The conditions that may lead to recriticality are therefore the principal objects of current investigations. Postaccident fuel containment (PAFC) analyses are being performed to assess the capability of the design and to identify potential modifications that could improve the molten fuel containment capability.

3.4.1. Safety Program Definition and Integration

A review of the LMFBR safety program has been initiated to determine whether the line of assurance (LOA) framework for the LMFBR program should be adopted for the GCFR safety program plan and whether a closer correlation of the program tasks could lead to better interfacing between the two programs. In general, the LMFBR and GCFR safety programs are quite compatible. The major difference is that the GCFR program does not use the LOA terminology, and the design basis task in the GCFR safety program includes only probabilistic targets but not dynamic response criteria. Table 3-6 compares the tasks of the two programs.

The following preliminary conclusions have been reached:

1. The LMFBR definition of LOA-1 and LOA-2 activities is more encompassing than the corresponding definition for GCFR safety activities. Whereas the current GCFR safety program includes only reliability-related objectives for LOA-1 and LOA-2, the LMFBR program also includes plant dynamic response and safety-related performance criteria (these tasks appear elsewhere in the GCFR development program). Furthermore, the LMFBR tasks include work packages on control systems, testing and maintenance, and operator response, which become of increasing interest at more advanced stages of development.
2. LOA-1 and LOA-2 tasks are design specific. The benefit of these tasks to the GCFR depends largely on the applicability of specific

TABLE 3-6
COMPARISON OF LMFBR AND GCFR SAFETY PROGRAMS

LMFBR Task	GCFR Task
LOA integration	Definition studies
LOA-1 accident prevention	Probabilistic technology
LOA-2 limit core damage	Core accident technology
LOA-3 Containment integrity	Primary vessel containment technology, secondary containment technology
LOA-4 radiological attenuation	Secondary containment technology

LMFBR system and component designs to the GCFR. It is not the scope of this task to evaluate the feasibility of adopting specific LMFBR designs.

3. The GCFR safety program depends on development of LMFBR methodology and data for LOA-3 and LOA-4 activities.
4. LMFBR program method and data development is largely applicable to the GCFR program except for sodium-related or design-specific aspects.
5. Practically all analysis efforts (i.e., dynamic response, reliability, fuel behavior, accident progression, consequences, risk) are both design and concept specific and need to be done as an integral part of the GCFR program.

The next phase in this evaluation will investigate an LOA organization of the GCFR safety program.

3.4.2. Probabilistic Technology

3.4.2.1. Residual Heat Removal System Reliability for the Two-Loop Demonstration Plant. Reliability analyses in support of the design effort on the two-loop demonstration plant continued. The two-loop plant is designed to utilize the same size components as the commercial plant. Two heat removal train configurations were compared:

1. Integrated two-loop main cooling system (MCS) and shutdown cooling system (SCS) and three CACS loops. The CACS for this configuration was assumed to employ a boiling CAHE instead the former design in which the CAHE was pressurized to prohibit boiling. The active components in the CACS loops are the primary coolant circulators, the auxiliary loop isolation valves (ALIVs), and the louvers on the air-cooled condensers. The CAHE steam drums are assumed to have pressure relief valves and water makeup valves.

2. Independent SCS circulator with the split steam generator (SG) concept. In this design concept, only the steam generator is common to an MCS/SCS loop.

Reliability calculations were made for the heat removal train and electrical subsystems of the RHR system and were based on common cause failure estimates. Table 3-7 shows the system failure probability per year (i.e., the system unreliability). The boiling CAHE did not significantly reduce the unreliability from the reference nonboiling CAHE. Furthermore, the RHR unreliability increased by a factor of five when it was assumed that one MCS loop was unavailable during refueling owing to scheduled maintenance. The design concept employing the independent SCS circulator and split steam generator is more reliable by about two orders of magnitude than the integrated MCS/SCS design concept.

3.4.2.2. Residual Heat Removal System Reliability for the Three-Loop Demonstration Plant. Reliability analyses were performed for a three-loop and a two-loop demonstration plant. The failure probability per year was determined for the heat removal train and electrical subsystems of the RHR system based on common cause failure estimates. The following cases were considered:

1. The reference case based on an integrated three-loop MCS/SCS and three-loop CACS. Both forced and natural convection in the CACS loops can cool the core.
2. The reference case modified to allow a boiling SCS with natural convection in either the SCS primary coolant loop, the SCS primary and secondary coolant loops, or the SCS primary, secondary, and tertiary coolant loops.
3. One MCS undergoing scheduled maintenance during refueling.
4. Only the CACS loops available during the first 24 h of a $<650\text{-mm}^2$ depressurization accident.

TABLE 3-7
ESTIMATED COMMON CAUSE RELIABILITY ESTIMATES FOR RHR SYSTEM (HEAT REMOVAL TRAINS
AND ELECTRIC POWER SUPPLIES) FOR VARIOUS TWO-LOOP DEMONSTRATION
PLANT CONCEPTS(a)

Configuration	Failure Probability (Q) Per Year			
	Reference		Boiling CAHE	
	All MCS for Refueling	One MCS Out for Refueling	All MCS for Refueling	One MCS Out for Refueling
Integrated 2 MCS/SCS and 3 CACS loops (FC + NC in CACS)	3.4×10^{-5}	1.3×10^{-4}	3.2×10^{-5}	1.2×10^{-4}
Integrated 2 MCS/SCS and 3 CACS loops (FC + NC in CACS); independent SCS circulator and split steam generators	7.4×10^{-7}	1.5×10^{-6}		

(a) FC = forced convection; NC = natural convection.

(b) Assumptions: (1) 5-h delayed repressurization for normally depressurized conditions; (2) NC in the secondary and tertiary loops adequate for FC and NC in the primary loop; (3) MCS normally used with SCS and CACS as backups, except for containment isolation events.

5. The MCS and CACS available during the first 24 h of a $<25.4\text{-mm}^2$ depressurization accident.

Table 3-8 shows the results of the reliability analyses. The RHR system unreliability is expressed in terms of the failure probability per year. The three-loop plant is more reliable by over one order of magnitude than the two-loop plant for the reference case with one MCS loop out for maintenance during refueling. Table 3-8 also indicates that the natural convection SCS concept significantly reduces the unreliability of the RHR system compared with the reference case. The assumption of natural convection in the SCS loop is the major factor behind the reduction in unreliability, since natural convection in the SCS eliminates the need for forced cooling and reliance on the main circulators. In addition, the capability of natural convection in the SCS loop greatly reduces the dependence on the ALIVs.

The three-loop plant with the boiling SCS has an estimated failure probability of $3 \times 10^{-7}/\text{yr}$, which is very close to the target reliability of $1 \times 10^{-7}/\text{yr}$ for RHR trains and power supplies. If, in addition to assuming that the CACS is available for the first 24 h after a slow ($<650\text{-mm}^2$) depressurization accident it is also assumed that the MCS is available, the unreliability is about $8 \times 10^{-9}/\text{yr}$. This value is well within the target reliability for the RHR system.

3.4.2.3. Common Cause Methods Development. RHR reliability analyses have generally considered the probability effect of common cause failures based on the "beta factor" method (Ref. 3-5). Three assumptions have been made:

1. No intersystem common cause failure coupling was analyzed.
2. Whenever a condition occurred while a specific redundant system was starting or running, all redundant loops in that system were assumed to fail simultaneously, with a probability which is β multiplied by the failure for a single loop.

TABLE 3-8.
ESTIMATED COMMON CAUSE RELIABILITY FOR RHR (HEAT REMOVAL TRAINS AND ELECTRIC
POWER SUPPLIES) FOR TWO- AND THREE-LOOP DEMONSTRATION PLANTS

Concept	Failure Probability (Q) Per Year		
	2-Loop Plant		3-Loop Plant
	2 MCS/SCS During Refueling	1 MCS/SCS Out During Refueling	1 MCS/SCS Out During Refueling
Reference	3.4×10^{-5}	1.3×10^{-4}	5.9×10^{-6}
Boiling SCS with NC in			
Primary loop only ^(b)	8.6×10^{-7}	3.6×10^{-6}	3.0×10^{-7}
Primary and secondary loops ^(b)	7.4×10^{-7}	3.1×10^{-6}	3.0×10^{-7}
All three loops ^(b)	7.2×10^{-7}	2.9×10^{-6}	3.0×10^{-7}
Primary loop only ^(c)	5.7×10^{-7}	3.3×10^{-6}	7.9×10^{-9}
Primary and secondary loops ^(c)	4.5×10^{-7}	2.9×10^{-6}	7.7×10^{-9}
All three loops ^(c)	4.3×10^{-7}	2.6×10^{-6}	7.6×10^{-9}

(a) NC = natural convection.

(b) Assumes that only the CACS can be used during the first 24 h of a $<25.4\text{-mm}^2$ depressurization accident.

(c) Assumes that the MCS and CACS can be used during the first 24 h of a $<25.4\text{-mm}^2$ depressurization accident.

3. All equipment subject to common cause failure was assigned the following generic beta factor values: for start failures, $\beta = 0.1$; for run failures, $\beta = 0.01$.

Each assumption limits the adequacy of the modeling analysis approach, which affects the accuracy of the predicted RHR failure rates. The first assumption yields an underestimate of RHR failure rates, and the second assumption overestimates these rates. The effect of the third assumption cannot be predicted, but is consistent with the data observed in current operating systems (Ref. 3-5).

To improve the accuracy of predicting the RHR failure rates, a more realistic modeling analysis approach must be developed for common cause failures. A three-task program was generated to meet this need:

1. Data survey.
2. Literature survey.
3. Methods development.

The first task was initiated by obtaining relevant data from the United Kingdom. The Fort St. Vrain data search (Section 3.4.2.4) has identified some potential common cause failures. A literature search was also begun which is expected to reveal potential data sources and to provide insights that will help in developing improved methods and models. Work on the methods development task is in the formative stage.

3.4.2.4. Reliability Data Development. Compilation of Peach Bottom shutdown data is complete, and pressurized water reactor (PWR) shutdown data are being categorized into initiating event types and reviewed for common cause failures.

Preliminary classification of unusual and circulator-related occurrences in British gas-cooled reactors and circulator-related has been

completed. Fort St. Vrain daily status reports are being compiled to provide data on failure rates, initiating event frequencies, and common cause failures for comparison with British experience.

3.4.3. Core Accident Technology

This quarter's effort emphasized methods development for the unmitigated GCFR response to a total LOSC and development of the experimental plan for natural circulation and steel melting test FLS-3 at Los Alamos Scientific Laboratory (LASL). The heat transfer and material relocation models in SCORIA (Ref. 3-6) are being coupled to a neutronics methodology for the determination of criticality conditions and postcriticality consequences. The STEFINS program (Ref. 3-7) for the calculation of molten steel solidification during an LOSC has been extended to include molten steel solidification caused by contact with flowing helium.

A series of out-of-pile and in-pile experiments has been proposed. The purpose of the experiments is to qualify LMFBR irradiated fuel for use in GRIST-2 and to resolve outstanding safety issues for core disruptive accidents. LOSC integral tests were also suggested.

3.4.3.1. Unmitigated GCFR Response to a Loss of Cooling Shutdown Accident.

The LOSC accident is a postulated accident beyond the plant design basis. It is hypothesized that all forced and natural circulation capability for decay heat removal is lost to the extent of core heatup to cladding melting. A mechanistic quantification of the LOSC accident sequence of events is being undertaken. In general, such quantification involves computer codes able to describe material relocation, heat transfer, phase change, structural mechanics, helium thermal-hydraulics, and neutronics. A code system which couples the SCORIA heat transfer and material relocation models to the COBRA (Ref. 3-8) fluid dynamics code has been developed.

During this quarter, development of a transient methodology for describing the approach to criticality and the postcriticality accident was initiated. A quasi-static two-dimensional space-time neutron kinetics procedure using the 2DB (Ref. 3-9) and PERT (Ref. 3-10) codes has been adopted

to couple SCORIA with the VENUS-II code (Ref. 3-11). This will provide an integrated neutronics, heat transfer, material motion, and disassembly LOSC analysis capability. The procedure shown in Fig. 3-4 is intended to continuously update power and reactivity effects, including thermal expansion, material relocation, phase change expansion, and Doppler feedback. Before criticality, SCORIA and a point kinetics routine do iterative calculations to obtain power for SCORIA and reactivity for the point kinetics routine. The interface code is SCORED.

Reactivity tables will be updated upon satisfying a certain criterion by using 2DB for the updated neutron and adjoint flux and PERT for the material reactivity worths. The core configuration for 2DB and PERT will be derived from SCORIA using SCORED. In addition, the Doppler effect will be included by adjusting the macroscopic cross sections using an interpolation of temperature-dependent cross section tables after each SCORIA iteration. Thermal and phase change volume expansion and material relocation reactivity effects will be estimated from the PERT worth distributions. When the reactivity insertion has overcome the shutdown margin and is in excess of prompt critical, the procedure will provide the power distribution, core configuration, and reactivity versus time functions necessary for the disassembly calculation in VENUS-III.

In choosing this particular quasi-static approach, accuracy, ease of implementation, methods development time, and applicability to LOSC conditions were considered. A literature search for the most appropriate methodology included review of (1) modal (Refs. 3-12, 3-13), nodal (Ref. 3-14), synthesis (Refs. 3-15, 3-16), variational (Refs. 3-17, 3-18), and other weighted residual methods (Ref. 3-19); (2) "exact" space-time diffusion theory numerical solutions (Refs. 3-20, 3-21); (3) "improved" quasi-static approximation (Refs. 3-22, 3-23); and (4) "adiabatic" quasi-static approximation (Refs. 3-24 through 3-26). The modal, nodal, synthesis, and weighted residuals methods (including variational techniques) involve a large, long-term development program to produce a feasible methodology for the two-dimensional and highly distorted core configurations. Exact diffusion

Fig. 3-4. Calculational flowchart for consequence determination

theory requires excessive computer time in two-dimensional multigroup computations and is thus not practical. The improved quasi-static approach is programmed in the FX2 code (Ref. 3-23). The major difference between the improved and the adiabatic approximation is that the improved approach calculates the delayed neutron precursor shape function in addition to its magnitude, and the adiabatic model assumes that the precursor shape is the prompt neutron shape. Since the predominant reactivity insertion mechanism is fuel motion, the precursor concentration is likely to be well approximated by the prompt neutron shape. Therefore, the added effort of adapting FX2 is not warranted, and the adiabatic approximation using the 2DB and PERT codes and a point kinetics routine was chosen.

3.4.3.2. STEFINS Method Development. The effect of helium flowing past molten steel as it solidifies is relevant to the analysis and interpretation of experimental data from FLS tests and steel melting and relocation tests (SMART) (Ref. 3-27). The one-dimensional heat transfer equations for the propagation of a solidification front across flowing or stationary molten steel in the presence of helium flow have been derived, solved, programmed, and debugged in STEFINS. Figure 3-5 shows the four-region phase change heat transfer problem solved in slab geometry by STEFINS. STEFINS also solves this problem in cylindrical geometry. The heat transfer equations and conditions are as follows:

Region 4

$$\frac{\partial T_o}{\partial t} = \alpha \frac{\partial^2 T_o}{\partial x^2} ,$$

$$\left. -k \frac{\partial T_o}{\partial x} \right|_{x=0} = h_h (T_{HE} - T_c) .$$

Fig. 3-5. STEFINS slab geometry model

Region 3

Right half

$$\left. \rho \lambda \frac{d\delta_4(t)}{dt} = k \frac{\partial T_4}{\partial x} \right]_{x=\delta_4(t)} - h_s (T_\infty - T_m)$$

Left half

$$\left. \rho \lambda \frac{d\delta(t)}{dt} = k \frac{\partial T_2}{\partial x} \right]_{x=\delta(t)} - h_s (T_\infty - T_m)$$

Region 2

$$\frac{\partial T_2}{\partial t} = \alpha \frac{\partial^2 T_2}{\partial x^2},$$

$$\left. \frac{\partial T_2}{\partial x} \right]_{x=0} = \left. \frac{\partial T_3}{\partial x} \right]_{x=0}$$

Region 1

$$\frac{\partial T_1}{\partial t} = \alpha \frac{\partial T_1}{\partial x^2} + q_\gamma,$$

$$T_1(0,t) = T_2(0,t) = T_w(t),$$

$$\dot{q}_w'' = -k \frac{\partial T_1}{\partial x} \Big|_{x=-w}$$

The terms in these equations are defined as follows:

ρ = steel density,

γ = steel heat of fusion,

k = steel thermal conductivity,

α = steel thermal diffusivity,

$\dot{q}_w''$ = incident or rejected heat flux boundary condition,
 w = wall thickness,
 t = time,
 $\delta(t), \delta_4(t)$ = solidified steel layer thickness,
 $l(t)$ = width of flowing molten steel region,
 T_∞ = bulk molten steel temperature,
 T_m = steel solidus temperature,
 T_c = temperature of interface between flowing helium and solidified steel,
 T_w = temperature of interface between wall and solidified steel,
 T_{He} = bulk helium temperature,
 h_h = heat transfer coefficient from helium to solid steel layer,
 h_s = heat transfer coefficient from molten steel to solid layer.

These equations are solved for δ , δ_4 , T_w , and T_c by assuming reasonable temperature profiles for T_0 , T_2 , T_3 and integrating the equations using a fourth-order Runge-Kutta technique. The approach is similar to that recommended in Ref. 3-28. The code requires (1) a known temperature for a surface external to the wall, (2) helium temperature and velocity, and (3) all dimensions. The code will calculate the molten steel velocity and temperature at the point of interest by specifying film or slug flow (whether the velocity is limited by mass flow rate or friction) and the steel temperature at the channel inlet.

The results of parametric studies using temperature profiles and helium flow rates typical of FLS-2 indicate that a laminar helium flow solidifies a laminar flow of molten stainless steel film at least an order of magnitude more slowly than solidification caused by heat transfer to the cladding.

3.4.3.3. Core Disruptive Accident Phenomenological Test Recommendations.

The use of LMFBR irradiated fuel in GRIST-2 tests for the GCFR safety program could constitute a major cost saving for the GRIST-2 experiment program. A pre-GRIST experimental program to qualify LMFBR irradiated fuel for

GRIST experiments has been studied. This program would use existing experimental facilities as much as possible. Table 3-9 summarizes the types of tests and the fuel required for comparing LMFBR irradiated fuel and restructured fuel with GCFR irradiated fuel for GRIST-2 transient tests. Pre-and post-transient microstructural fuel and cladding characterization is recommended for a fundamental understanding of any transient response differences found in transient tube burst tests, unprotected loss of flow (LOF) simulated transient tests, and unprotected transient overpower (TOP) simulated transient tests. These transient tests could be conducted using the tube burst facility at Hanford Engineering Development Laboratory (HEDL), the GCFR direct electric heating apparatus at ANL, and the transient reactor test facility (TREAT) without the GRIST-2 facility. To obtain the flow needed for TOP tests, a complete qualification program would require a TREAT test facility equivalent to the R-series facility used for LMFBR transient testing. Since this may be an unacceptable financial burden on the program, these tests could be deferred until the GRIST-2 facility is installed in the TREAT Upgrade reactor. Waiving the requirement for cladding failure location and time comparisons is not recommended. Seventeen irradiated rods and eight unirradiated rods are estimated to be required for the fuel conditioning comparison studies.

The decision for an upflow, bottom-supported core and recent GCFR analytical and experimental findings have necessitated a review of CDA phenomenological uncertainties which are amenable to experimental resolution. This review excludes the SMART and GRIST programs and updates the safety program plan (Ref. 3-29). The phenomenological and integral test and analytical efforts are directed toward limiting the accident consequences.

The major phenomena issues of an LOSC accident are related to recriticality. The most important issues are concerned with fuel column stability after cladding melting but before fuel melting and means of mitigating accident consequences and terminating the accident. Fuel packing fractions must be determined from postulated fuel crumbling, a molten fuel slumping criterion must be established, and the cladding flow initiation and flow mode must be investigated.

TABLE 3-9
FUEL REQUIREMENTS FOR GRIST-2 FUEL QUALIFICATION TESTS

Type of Fuel	Use	Designation	No. of Rods
Fresh	Characterization	F	1
Fresh cladding	Rupture tests	RB	1
Restructured	Characterization	R	1
Restructured	TOP tests	RT	2
Restructured	LOF tests	RL	3
Irradiated(a)	Characterization	I	2
Irradiated(a)	Cladding rupture tests	IB	2
Irradiated(a)	TOP tests	IT	5
Irradiated(a)	LOF tests	IL	8

8

17

(a)3% to 5% burnup.

The most important pretransition phase phenomenon in the unprotected LOF accident is fuel and steel relocation, which will be investigated in GRIST-2. ANL thermite technology will allow out-of-pile investigation of the mode of fuel removal from the core, i.e., by either gravity drainage or fuel blowout through the axial blankets. Analysis at ANL has shown that the recriticality transient is sensitive to the relative timing of radial rod swelling and expansion to axial fuel motion. The timing and magnitude of radial solid-state swelling and phase change expansion under loss of flow heat-up conditions may be investigated by direct electric heating tests.

The most important TOP accident issues are related to cladding failure location and the ability of fuel to be swept out of the core without plugging coolant channels. Cladding failure would be adequately addressed by GRIST fuel qualification. The CAMEL tests at ANL (Ref. 3-30) have been studying fuel sweep-out in LMBFR bundles with sodium flow and injected thermite. A similar facility for GCFR bundles with spacer grids and full helium flow is recommended for GCFR fuel sweep-out investigations. Twelve irradiated and ten unirradiated rods are estimated to be required for these phenomenological tests.

3.4.3.4. LOSC Integral Tests Recommendations. The decision for an upflow, bottom-supported core and recent GCFR analytical and experimental findings indicate a possible need for LOSC accident integral experiments. These experiments may be in-pile, such as those in GRIST-2, or they may be out-of-pile with irradiated fuel. Hence, experimental needs must be determined so that a decision can be made on the experimental facility.

The analytically supported phenomenological and integral test programs will be an aid in limiting core damage, fuel vaporization, and accident-induced work potential, which may be accomplished by eliminating the first or subsequent recriticalities or limiting the potential magnitude of the power bursts. Table 3-10 suggests the issues requiring integral testing.

3.4.3.5. Transient SCORIA/COBRA Analysis for FLS-2. The FLS-2 experiment performed at LASL involved electrical heating of 37-rod GCFR fuel bundle at

TABLE 3-10
SUGGESTIONS FOR LOSC INTEGRAL TESTS

Issue	Required Test	Prerequisite Phenomenological Test
Fuel column stability and ultimate collapse mode	A single subassembly (61 rods may be adequate) with a duct at prototypical LOSC temperatures will be heated at decay heat level, and fuel motion initiation, material relocation, and fuel column collapse will be observed. The phenomena of interest are as follows: fuel column bowing, swelling, molten fuel foaming, solid fuel column collapse, solid fuel motion, molten fuel column collapse, and packing density of collapsed solid fuel (if any). The tests will be run at high and low pressure with irradiated fuel.	Direct electric heating tests on fuel column strength and fuel swelling, foaming, and slumping Single-rod bowing tests against a cold duct wall Modeling of CCFR assembly with simulated fuel pellet stacks for packing fraction estimates
Burst and postburst fuel removal and fuel/steel thermal interaction(a)	A single subassembly (37 rods may be adequate) with a duct will be heated to simulate an LOSC criticality excursion (after cladding melting) and postexcursion heat deposition. Particular attention will be focused on fuel removal through the upper axial blanket, melt-through of steel blockages and duct walls, and fuel drainage through the lower axial blanket. Tests will be run at high and low pressures. Unirradiated fuel may be adequate if unacceptable preburst neutronic heating can be established.	Upper axial blanket injection tests using thermite ANL molten fuel pool experiments for axial and radial steel melt-through rates Depressurized thermite drainage tests in simulated lower axial blanket bundles
Postburst mechanical work potential and aerosol particulate size distribution(a)	A single subassembly (37 rods may be adequate) with a duct at prototypical LOSC temperatures will be heated to simulate an LOSC criticality excursion. Particular attention will be focused on upper axial blanket and duct wall distortions to establish the thermal to mechanical work efficiency. The posttest examination will include studying the residual fuel particles which escape from the upper axial blanket to determine particle size distribution. The tests will be run at high and low pressures. Unirradiated fuel may be adequate.	None

(a) In-pile test may be required.

a decay heat well beyond cladding and duct wall melting to demonstrate the capability of the LASL heater rods to sustain the conditions anticipated in follow-on SMART experiments. Post-FLS-2 analysis has concentrated on the bundle heat-up transient from room temperature to cladding melting.

The SCORIA/COBRA analysis of the FLS-2 experiment (Ref. 3-27) was terminated after 2 min (real time) because certain analytical predictions and experimental results diverged significantly prior to cladding melting. A reanalysis of the same experiment was necessary to evaluate the cause of the divergence.

The reanalysis included additional detail in the upper test assembly structure and several data and calculation changes which were expected to either increase the agreement with the experiment or reduce the cost of calculation. The reanalysis was carried through the time of incipient cladding melting at approximately 4 min. Future analysis would incorporate modeling of cladding melting and freezing with modeling of natural convection in COBRA. This is not part of the FLS-2 analysis.

Better agreement was noted in most areas, and was particularly good in the core region. Agreement in the upper blanket still ranges from moderately good to poor. The mechanisms considered most responsible for the disagreement in the upper blanket are axial conduction and multirow radiative heat transfer.

The importance of these two phenomena may be peculiar to the FLS-2 experiment, in which natural-convection-induced flow was intentionally blocked at the top of the axial blanket to maximize the severity of the heater condition. Thus, the natural-convection-induced flow was forced to reverse at the top of the axial blanket, a condition which is not normally expected and which will not be simulated in future experiments, since in FLS-2 the heater rods have demonstrated the ability to maintain continuous heating over the required range of conditions.

3.4.3.6. FLS-3 Test Specification. A joint GA/LASL test recommendation for the FLS-3 experiment has been proposed and a test specification developed (Ref. 3-31). The three basic purposes of the FLS-3 experiment (listed in their sequence of accomplishment) are as follows:

1. To obtain experimental information on natural circulation with an externally cooled loop. This information will be used for preliminary characterization of natural circulation effects, identification of experimental limitations, and development of a natural circulation verification plan.
2. To obtain information on the capability for stable cooling of a heated, unfueled, steel-clad bundle at the faulted temperature limit.
3. To obtain experimental verification of a bottom-supported bundle design and of continued helium flow during the destructive phase of SMART experiments and to obtain preliminary information on steel relocation in a continuing bundle flow configuration.

The test objectives for each experimental phase are listed below:

1. Natural convection tests (FLS 3-1 through 3-18).
 - a. Measure fuel rod thermal response and helium mass flow response due to natural circulation following a step change in power.
 - b. Measure steady-state helium mass flow rate and cladding temperature due to natural circulation as a function of heat sink elevation, helium pressure, and core power level.
 - c. Measure coolant inertia effects during natural circulation flow development from stagnant conditions at a predefined temperature level.

2. Faulted temperature limit tests (FLS 3-19 to 3-22).
 - a. Investigate the stability of the bundle geometry during steady-state FLS-3 bundle operation at the faulted temperature limit (maximum cladding temperature = 1300°C) and low helium pressure.
 - b. Demonstrate heater rod capability for DACT tests.
 - c. Provide guidance in the development of DACT test program.
3. Destructive phase test (FLS 3-23)*
 - a. Investigate the experimental limitations of maintaining a continuous helium flow during the destructive phase. To satisfy SMART test objectives with respect to steel blockage characterization, it is very likely that continued helium flow during heat-up and cladding melting will be necessary.
 - b. Verify bottom-support bundle design features. The SMART test bundle design has been changed to a bottom support design.
 - c. Determine the first-order effects of steel relocation and freezing under helium flow conditions. The steel blockage geometry and location may be influenced by the continued helium flow.
 - d. Obtain experimental data for determining the cladding melting time delay due to continued helium flow.

*The principal objective of this test is to investigate the effects of a change to an upflow core.

- e. Demonstrate long-term heater rod integrity for much slower bundle heat-up rates compared with FLS-2.

Figure 3-6 shows the FLS-3 test hardware configuration. The three primary components are the test vessel, the heat exchanger, and the trailer helium supply. These are connected by piping in such a way that the experiment can be operated in either a closed-loop natural circulation mode (flow configuration 1) or a once-through forced-flow mode (flow configuration 2). The flow configurations are defined by the position of the three helium valves, V1, V2, and V3.

The natural circulation flow mode is used for test phase A, which comprises FLS tests 3-1 to 3-18. Valve V1 is open and valve V3 is closed. The system is pressurized to test pressure through valve V2, which remains closed during a given test. Hot helium rises by natural convection from the low-elevation heat source (test vessel) to the high-elevation heat sink (heat exchanger). The hot helium piping is internally insulated. The heat exchanger is an existing unit consisting of a straight helium pipe with a helical flow cooling water jacket on the outside. Cold helium returns to the bottom of the test vessel through the helium shutoff valve V1 and the helium flow meter. For all tests, maximum cooling can be supplied to the heat exchanger to yield as low a cold helium return temperature as possible.

The major parameter to be varied in this configuration is the elevation difference ΔH between the core midplane elevation in the test bundle and the midplane elevation of the heat exchanger. Two reference values for the elevation difference have been selected:

<u>Test</u>	<u>ΔH</u>
FLS 3-1 to 3-9	As large as possible within the high high bay (approximately 5 to 6 m)
FLS 3-10 to 3-23	1 to 3 m

Fig. 3-6. Configuration of FLS-3 experiment

The once-through forced flow mode is used for test phase B, faulted temperature tests (FLS tests 3-19 to 3-22) and test phase C, destructive test (FLS test 3-23). The helium recirculation valve (VI) is closed, and the helium trailer flow valve (V2) and helium back pressure valve (V3) are open. Helium flows from the trailers through the helium trailer/flow valve (V2), through the choked flow control nozzle and the helium flow meter, to the test vessel inlet. Heated helium leaving the top of the test vessel flows to the heat exchanger as in flow configuration 1. Cooled helium is released to the atmosphere through the helium back pressure valve (V3). Bypass leakage through the closed helium recirculation valve (VI) will be minimized to a small fraction of the flow through the test vessel, but absolute leak-tightness is not required. Flow control is accomplished either by the helium trailer flow valve (V2) being operated as a variable choked flow control valve or by a fixed choked flow nozzle sized for destructive test FLS 3-23. In either case the objective is to control flow by a nozzle which operates in a choked condition for all test pressure levels up to 30 atm. In this flow control mode the helium back pressure valve (V3) is adjusted to establish the pressure for tests FLS 3-19 to 3-23.

Table 3-11 gives the FLS 3 test sequence sequence. The test pressure is positively established at the beginning of a test. The reference temperature is the target for maximum cladding temperature in the test bundle. The test power (and the mass flow for test phases B and C) will be determined on the basis of pretest analysis. Fine adjustments of power during a test so that the reference temperature can be reached will not be necessary (and for test phase A are not desirable). However, the power of any test should be adjustable on the basis of extrapolation from previous tests, i.e., the test power for test 7 should be adjustable on the basis of extrapolation from tests 1 and 6.

3.4.4. Primary Containment Technology

3.4.4.1. Postaccident Fuel Containment: PCRVR Penetration Analysis. The GROWS-2 computer code (Ref. 3-32) is capable of analyzing the growth of internally heated core debris pools into reactor cavity structures and has

TABLE 3-11
FLS 3 TEST SEQUENCE

Test	Flow Mode(a)	VI	V2	V3	Reference Temperature (°C)	Reference Pressure(b) (atm)	Mass Flow (kg/h)	Test Power (kW)(c)
Phase A								
3-1	Natural	Open	Closed	Closed	500	65	Self-regulated	TBD
3-2	circulation					30		TBD
3-3	$\Delta H = \text{maximum}$					2		TBD
3-4					750	2		TBD
3-5						30		TBD
3-6						65		TBD
3-7					1000	65		TBD
3-8						30		TBD
3-9						2		TBD
3-10	Natural				500	65		TBD
3-11	circulation					30		TBD
3-12	$\Delta H = 1 \text{ to } 3\text{m}$					2		TBD
3-13					750	2		TBD
3-14						30		TBD
3-15						65		TBD
3-16					1000	65		TBD
3-17						30		TBD
3-18						2		TBD

TABLE 3-11 (continued)

Test	Flow Mode(a)	V1	V2	V3	Reference Temperature (°C)	Reference Pressure(b) (atm)	Mass Flow (kg/h)	Test Power (kW)(c)
Phase B								
3-19	Trailer	Closed	Control	Control	500	2	(15)	TBD
3-20	flow	↓	↓	↓	750	2	(15)	TBD
3-21	↓	↓	↓	↓	1000	2	(15)	TBD)
3-22	↓	↓	↓	↓	1300	2	(15)	TBD)
Phase C								
3-23	Trailer flow	Closed	Control	Control	Cladding melting	(13)	(15)	37

(a) Fig. 3-6.

(b) Numbers in parentheses are preliminary.

(c) TBD = to be determined.

been adapted for ex-vessel PAFC studies. Early PCRV penetration analyses for the 300-MW(e) GCFR at GA (Ref. 3-33) used an equal heat flux model and at ANL, the original GROWS-2 code (Refs. 3-34, 3-35). Table 3-12 presents the results of these analyses. The calculation in Ref. 3-34 was reproduced in Ref. 3-35 using a slightly improved version of GROWS-2 (Phase II). The results indicate a reduced sideward penetration and an enhanced downward penetration. This effect is even more pronounced in the case of a 6-h delay for melt-through than in the case of a 1000-s delay.

An improved version of GROWS-2 for the gas release calculation has been generated by ANL. Two sets of results, one with 1000-s and the other with 6-h delay times, were obtained with the new gas release model (Phase III). These results, shown in Table 3-12, are more consistent than previous ones. For the case of 6-h delayed melt-through, the time for the molten pool to reach the first row of PCRV tendons is 28 h, and the penetration of the PCRV lower head takes 27 days. These results show slightly faster pool growth than those of Ref. 3-33; however, the time for the molten pool to reach the first row of tendons is still sufficiently long to effect PCRV depressurization.

3.4.4.2. Postaccident Fuel Containment Assessment for the 1200-MW(e) GCFR.

A preliminary assessment of a molten fuel containment system for the 1200-MW(e) GCFR commercial plant was prepared. Three cases of meltdown conditions were assumed. Case 1 assumed that the initial core melting sequence resulted in melting of the central portion of the grid plate and all the material supported by the grid plate out to the interface between the core and the radial blanket assemblies. Case 2 assumed melting of the first row of blanket assemblies, and case 3 assumed additional melting of the second row of radial blankets. Case 2 represents the current best estimate. The calculated minimum core debris volume of case 1 is 43 m^3 , that of case 2 is 52 m^3 , and that of case 3 is 62 m^3 .

TABLE 3-12
PCRV PENETRATION IN A 300-MW(e) GCFR

Calculational Case	Failure of First Row of PCRV Tendons (h)	Melt-Through Full Thickness of PCRV Lower Head (days)
Phase I		
1000-s delay(a)	12	38
6-h delay(b)	31	48
Phase II		
1000-s delay(c)	24	27
6-h delay(c)	120 days	15
Phase III		
1000-s delay (present analysis)	16	26
6-h delay (present analysis)	28	27

(a)Ref. 3-34.

(b)Ref. 3-33.

(c)Ref. 3-35.

Figure 3-7 shows a recommended molten fuel containment system configuration. The system fits in the lower PCRV cavity, and its outside diameter (8.84 m) is the PCRV cavity diameter of the 1200-MW(e) commercial plant. A combination of ceramic crucible, steel bath, and uranium metal bath concepts is recommended for molten fuel containment (Ref. 3-36). The permanent crucible adjacent to the cavity liner is a 0.23-m-thick MgO layer, which is the material and thickness suggested in Ref. 3-31. Pyrolytic graphite could also be chosen because of its low thermal conductivity direction normal to the plane of heat conduction. A layer of uranium alloy (0.04 m) is placed above the MgO to fill all the gaps and open pores of the MgO blocks upon melting and to prevent penetration of the lighter core melt. Above the uranium alloy there is a thick layer of stainless steel (0.53 m) which serves as sacrificial material to provide a 10-h heat capacity and to form a steel bath to promote sideward heat removal. The debris heights for the three meltdown cases are 1.04, 1.26 and 1.50 m.

The final molten fuel containment configuration is expected to be established at 10 h, when a quasi-steady-state condition is reached. The permanent MgO crucible contains both the core debris and materials melted from the wall of the molten fuel containment system. Molten fuel and blanket materials form the bottom layer, molten steel forms the second layer, and shielding and control materials form the uppermost layer.

The decay heat in the melt 10 h after shutdown is 25 MW, 40% of which is removed by thermal radiation upward through the graphite surface to the upper cavity. Thirty-one percent can be removed through the side wall and 29% through the bottom if the heat flux at the wall of the molten fuel containment system is uniformly distributed. This can be made possible by adjusting the MgO thickness. The uniform heat flux is 120 kW/m^2 at the cavity liner. The cavity liner temperature would be raised to 300°C , which is below the 450°C failure limit, and the 40% upward-flowing heat would require forced or natural helium convection cooling for a period of time. Helium cooling could be stopped when the upward-flowing heat was reduced to 15% to 20% of the decay heat in the melt.

Fig. 3-7. Molten fuel containment system proposed for 1200-MW(e) commercial plant.

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4.0 CIRCULATOR TEST FACILITY

The objective of this task is to design a facility for the development, qualification, and acceptance testing of GCFR primary cooling system components, including the main helium circulator and its drive motors and controllers, the circulator service system, the loop isolation valve, and portions of the thermal barrier. The scope of this task involves (1) evaluation of alternate test facility concepts in terms of technical feasibility and cost; (2) identification of the most promising test facility concept; (3) establishment of an architect/engineer conceptual design; and (4) final design, construction, modification, and checkout of the facility.

A conceptual design and cost estimate for a modified version of the existing HTGR circulator test facility showed that it could be used by the GCFR with minor modifications. Figure 4-1 shows the helium flow test loop used for testing the components of the primary cooling system. Figure 4-2 illustrates the installation of the circulator in the test vessel, and Fig. 4-3 shows how the test loop and its support equipment are integrated into the existing building.

The total estimated cost of modifying the HTGR circulator test facility is \$18.9 M in escalated dollars. This cost includes a 25% contingency and \$420 K in construction, planning, and design costs which were spent from 1978 to 1979.* The total estimated cost of the facility in October 1979 dollars is \$13.6 M. These costs represent a significant savings over the previous estimate of \$27.7 M in escalated dollars.

*This cost is based upon an engineering start date of October 1982, with completion of facility construction in July 1984.

Fig. 4-1. Helium circulator test facility with indirectly cooled external helium loop

Fig. 4-2. Helium circulator test facility motor and circulator

Fig. 4-3. Main helium circulator test facility, ground floor plan

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