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GAS-COOLED FAST BREEDER REACTOR

**QUARTERLY PROGRESS REPORT FOR THE PERIOD
AUGUST 1, 1979 THROUGH OCTOBER 31, 1979**

by
PROJECT STAFF

MASTER

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

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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 Layout

Figure 1-1 shows the PCRV arrangement for a 400-MW(e) demonstration plant. There are two top-exhaust steam generators, and the main helium circulators are in horizontal cavities in the bottom head. In addition, there are three core auxiliary heat exchanger (CAHE) cavities, with the CAHE bundle in the top head and the auxiliary circulator in the bottom head. These locations satisfy the natural circulation requirements. The core cavity is offset 3.20 m (10 ft 6 in.) to obtain the minimum PCRV size, and the resulting PCRV o.d. is 29.27 m (96 ft 0 in.). The height of the PCRV is 32.62 m (107 ft 0 in.) measured from the containment mat.

A comparative study was initiated for the six-loop 1200-MW(e) large plant to evaluate two PCRV configurations using either top or bottom exhaust steam generators (Figs. 1-2 through 1-6). Using the design geometry for the component and service cavities in Table 1-1, two layout drawings were generated for the six-loop PCRV. Figure 1-7 shows the PCRV arrangement using a top exhaust steam generator, and Fig. 1-8, a PCRV arrangement using a bottom exhaust steam generator.

The bottom exhaust steam generator is considered more practical for the manufacturer, however the PCRV configuration with a top exhaust steam generator (Fig. 1-7) is preferable because (1) the o.d. is smaller by 3.35 m (11 ft); (2) the ducting is simpler; and (3) the helium circulators and steam generators are in the same plane, making the PCRV support and the resulting tendon placement less complicated.

Fig. 1-1. Two-loop demonstration plant PCRV with top exhaust steam generator

Fig. 1-2. Six-loop PCRV with top exhaust steam generator

Fig. 1-3. Flow path for six-loop PCRVR with top exhaust steam generator

Fig. 1-4. Six-loop PCRV with bottom exhaust steam generator

Fig. 1-5. Flow path during normal operation for six-loop PCRV with bottom exhaust steam generator

Fig. 1-6. Flow path during natural circulation operation for six-loop PCRV with bottom exhaust steam generator.

TABLE 1-1
DESIGN GEOMETRY FOR GCFR SIX-LOOP COMMERCIAL PLANT

Component	Quantity	Top Exhaust Steam Generator(a)	Bottom Exhaust Steam Generator(b)
Operating pressure	—	10 MPa (1450 psi)	10 MPa (1450 psi)
Core cavity	1	8.84 m (29 ft) ϕ , 16.21 m (53 ft 2 in.)	8.84 m (29 ft) ϕ , 16.21 m (53 ft 2 in.)
Steam generator midheight diameter	6	5.74 m (18 ft 10 in.) (i.d. liner)	5.08 m (16 ft 8 in.) (i.d. liner)
Steam generator feedwater pipe	6	355.6-mm (14-in.) o.d. + insulation	355.6-mm (14-in.) o.d. + insulation
Steam generator feedwater pipe chase	3 6	1.83 m (6 ft) ϕ —	— 1.21 m (4 ft) ϕ minimum
CAHE diameter	3	3.66 m (12 ft) (i.d. liner)	5.41 m (17 ft 9 in.) (i.d. liner)
CAHE feed pipe	6	457.2-mm (18-in.) o.d. + insulation	457.2-mm (18-in.) o.d. + insulation
CAHE pipe chase	3	1.67 m (5 ft 6 in.) ϕ	1.67 m (5 ft 6 in.) ϕ
Main circulator diameter (horizontal).	6	4.16 m (13 ft 8 in.), 3.86 m (12 ft 8 in.) 3.10 m (10 ft 2 in.) (o.d. liner)	4.16 m (13 ft 8 in.), 3.86 m (12 ft 8 in.) 3.10 m (10 ft 2 in.) (o.d. liner)
CAHE circulator diameter (vertical)	3	4.44 m (14 ft 7 in.), 3.96 m (13 ft), 3.42 m (11 ft 3 in.) (o.d. liner)	4.44 m (14 ft 7 in.), 3.96 m (13 ft), 3.42 m (11 ft 3 in.) (o.d. liner)
Reactor plant cooling water system pit	3	2.43 m (8 ft) ϕ	2.43 m (8 ft) ϕ
Pressure relief pits	2	2.89 x 1.82 m (9 ft 6 in. x 6 ft)	2.89 x 1.82 m (9 ft 6 in. x 6 ft)
Steam generator closure	6	Concrete	Concrete
CAHE closure	3	Concrete	Concrete

(a) Fig. 1-7.

(b) Fig. 1-8.

Fig. 1-7. PCRV layout for six-loop GCFR with top exhaust steam generator

Fig. 1-8. PCRV layout for six-loop GCFR with bottom exhaust steam generator

An effort was made to reduce the o.d. [42.98 m (141 ft)] of the pressure vessel with the bottom exhaust steam generator (Fig. 1-8). Figure 1-9 shows the revised pressure vessel design produced by reduction of the CAHE cavity diameter at the top portion and optimization of the cooling water header pits. The resulting o.d. is 41.45 m (136 ft); the overall height remains 39.01 m (128 ft).

To assist Bechtel Corporation in its study of the containment mat for the large plant, displacement information at the PCR/V/containment interface was provided for the PCR/V configuration with the horizontally mounted helium circulator. The displacements were calculated for the interface between the outer edge of the pedestal and the containment mat and included the radial and tangential displacements for the plant conditions of initial prestress, operating temperature, and maximum cavity pressure. The displacements were generated by a three-dimensional finite element analysis based on a mathematical model of the PCR/V configuration for the earlier three-loop 300 MW(e) downflow core reference configuration modified to incorporate the pedestal support for the horizontally mounted helium circulator.

1.11.2. Stainless Steel Liner Study

The consequences of eliminating the outer radial shield and utilizing stainless steel for portions of the PCR/V liner were evaluated. A plant configuration without any radial shielding external to the core barrel was defined and the irradiation experienced by various components determined. Materials which could withstand the irradiation and alternative design approaches to reduce the radiation levels were examined. Figure 1-10 shows the plant configuration chosen for the initial evaluation. It is similar to the upflow design used in the upflow/downflow study (Ref. 1-1).

Fig. 1-9. PCR layout for six-loop GCFR with bottom exhaust steam generator

Fig. 1-10. PCRV configuration for stainless steel liner study

A shielding analysis was performed for the plant configuration shown in Fig. 1-10, and the results without the outer radial shield were compared with those obtained with the shield (Ref. 1-2). The effects of deleting the shield are as follows:

1. There is very little effect on the core restraint cylinder.
2. Total neutron doses to the thermal barrier, liner, and PCRV concrete adjacent to the liner increase by approximately 2-1/2 orders of magnitude to 6×10^{20} nvt at 30 effective power years.
3. Concrete gamma heating increases two orders of magnitude to 3.6 mW/cm³.
4. PCRV tendon lubricant doses increase to 10^{10} rads.
5. Total neutron doses to the PCRV rebar and tendons [assumed to be located 203 mm (8 in.) and 457 mm (18 in.) radially outward from the PCRV liner] increase to 1.5×10^{20} and 1.5×10^{19} nvt, respectively. The corresponding high-energy ($E > 1.0$ MeV) fluences are 1.5×10^{18} and 1.0×10^{17} nvt, respectively.
6. Activation fluxes on the steam generator and CAHE caused by thermal neutron streaming through the upper cross ducts result in exposures of 800 and 300 mrem/h.

The above values are nominal, and it is recommended that they be increased by a factor of two to allow for the preliminary nature of the analysis. Further shielding appears necessary to accomplish the following:

1. Limit the nominal total neutron fluence to the linear and thermal barrier cover plates to 5×10^{20} nvt (1×10^{21} nvt with an allowance for a factor of two) so that stainless steel would meet the radiation exposure criteria of Ref. 1-3.

2. Limit the gamma dose rate to the tendons to 10^9 rads.
3. Limit the activation dose rates on heat exchanger surfaces to 100 mrem/h, as recommended in Ref. 1-4.

Additional shielding analysis, largely reported in Ref. 1-3, showed that (1) an increase of 32 mm (1-1/4 in.) in the core barrel thickness to 76 mm (3 in.) would reduce the neutron fluence to the desired value of 5×10^{20} nvt; (2) 57 mm (2-1/4 in.) of lead shielding would decrease the gamma dose to the tendons to the desired value of 10^9 rads; (3) a third row of removable shielding would reduce the neutron fluence by a factor of 6; and (4) 102 mm (4 in.) of shielding would limit the dose rate on heat exchanger surfaces to the desired value of 100 mrem/h. Although these effects were considered separately and the combined effects were not evaluated, there would obviously be some cumulative effect.

Removal of the outer radial shielding created problems for the concrete, liner, and thermal barrier. For the concrete and liner portions, it will probably be necessary to revise the ASME, Section III, Division 2, Code. A stainless steel liner will probably be required, and the concrete backing may have to contain steel punchings as part of the coarse aggregate. Neither of these materials is currently permitted in the code, since its intent is to reflect current practice with thermal reactors. In general, appropriate supporting analysis and test results are required in order for the materials to be approved. Obtaining these data and guiding the changes through the various code committees would take a long time. In addition, there is little interest outside General Atomic (GA) in changing the subsection of the code applicable to PCRVs.

Problems involving the concrete center around (1) its neutron irradiation resistance, (2) its reinforcement and prestressing systems, and (3) the hardening effects of gamma radiation on prestressing system corrosion-inhibiting compounds. Reference 1-5 discusses the effects of radiation on concrete materials. In general, it is known that concrete

containing quartz (sand, sandstones, etc.) shows the beginnings of damage at a total neutron fluence of approximately 10^{19} nvt. This material would not be suitable for use at significantly higher fluences, and accordingly, a 610-mm (2-ft) region of special concrete would be required around the core cavity and possibly the cross ducts. This special concrete would contain steel punchings, crushed hydrated cement paste, or other special aggregates. Although there is evidence that such concrete would not be affected by fluences up to at least 10^{21} nvt, there is no reliable test data at such fluences to confirm its use. Such data would have to be obtained through a costly and possibly lengthy development program. Without these data, the chance that the concrete would be satisfactory can only be conservatively estimated at 50%. If the data showed that the concrete was not satisfactory at a fluence of 10^{21} nvt, one or more additional rows of removable shielding could be used to reduce the fluence to 10^{20} nvt, which would not significantly affect some of the potentially available aggregates. (This would only be done as a last resort since the entire plant would be affected.) The Code limit of 10^{21} nvt is construed as an upper limit, since there are no known data which justify this limit for most conventional aggregates.

References 1-6 and 1-7 discuss the problems associated with rebar and prestressing tendon exposure to neutron irradiation. The major concerns are keeping the high-energy neutron fluence ($E > 1$ MeV) within code limits and either limiting the nil ductility transition temperature shift or evaluating the effect of the shift on the safety of the vessel. It appears that moving the rebar and first row of tendons around the core cavity radially outward a maximum of 152 mm (6 in.) from 203 mm (8 in.) and 457 mm (18 in.) to 356 mm (14 in.) and 610 mm (24 in.), respectively, would substantially solve both problems, particularly if high-density shielding concrete were used adjacent to the liner. The gamma dose to the first row of tendons would also be reduced, so that the amount of lead shielding incorporated in the first row of tendons could be less than 25 mm (1 in.) or could possibly be completely eliminated. The effect of the special shielding concrete and

outward movement of the rebar and tendons has not been evaluated, but is likely to increase the PCRV diameter above the value shown in Fig. 1-10.

References 1-6 through 1-8 discuss the problems associated with a stainless steel liner. Most of the problems appear capable of solution; however, a considerable effort will be required to show that stress corrosion cracking will not occur in sensitized austenitic stainless steel. Regulatory Guide 1.36, which limits the fluoride and chloride content in insulation for stainless steel, will be imposed (it may even be imposed on the thermal barrier without a stainless steel liner). Regulatory Guide 1.44 identifies the concerns and special requirements for the use of stainless steel, but does not cover stabilized stainless steels such as type 321, because they are not used in light water reactors (LWRs). It is also more applicable to structures in tension or bending than in compression.

The choice of stainless steel material has not been made yet, but as suggested in Ref. 1-7, type 321 would probably be better than either type 304 or 316. Nevertheless, since the liner is permanent and not subject to in-service inspection, it may be necessary to demonstrate the resistance of the stabilized stainless steels to stress corrosion cracking and to specify more stringent controls on fabrication than are required for carbon steel. These material problems are also applicable to the thermal barrier and current shielding structures. However, shielding structures are not part of the reactor pressure boundary. There is also a small possibility that a carbon steel can be found which has a sufficiently low nil ductility transition temperature and small shift owing to irradiation, so that a stainless steel liner would not be required. This would alleviate many of the liner problems.

The question of nuclear heating was also addressed. Although the maximum rate of 3.6 MW/cm^3 requires additional embedded cooling tubes within the concrete, there is no feasibility problem associated with locating the tubes within the vessel.

A rough cost estimate shows that added PCRV costs balance the reductions due to core cavity decreases. Thus, the capital cost saving is the difference between eliminating the shielding and using stainless steel for the liner, or approximately \$3.7 million for the demonstration plant. This value is sufficiently uncertain that further study is necessary to determine a realistic cost difference. It is also felt that the risk and the development costs may not be significantly reduced if the radial shielding is eliminated, but may only be transferred from the shielding to the liner and concrete. It is concluded that the conventional ferritic liner with outer radial shielding should be retained as the main effort through completion of the conceptual design.

1.11.3. Core Cavity Closure Design and Analysis

The minimum clearance between the core cavity closure refueling and instrumentation tree penetrations was examined. The major problem is in cooling the penetration, since the heat loads are expected to be high and there is limited room for cooling tubes. If a satisfactory cooling tube arrangement cannot be found, it may be necessary to utilize a thermal sleeve at the base of the penetration. It should be possible to have a 127-mm (5-in.) space between penetrations if a thermal sleeve is not required. This is acceptable from an overall plant viewpoint.

A two-dimensional axisymmetric analysis of the core cavity closure for the commercial plant has been in progress to study the structural behavior of the closure and to assess the feasibility of designing a closure capable of supporting an operating pressure at and above 10 MPa (1450 psi). The variables considered include closure depth/diameter ratio, direction of support reaction, geometry of support region, perforated zone radius/closure radius ratio, and ligament efficiency. The results of the analysis indicate that the critical design stresses are the ligament stresses at the center of the closure on the top and bottom surfaces and the bearing stress in the support region. These critical stresses are sensitive to the support details and the direction of the support reaction. The horizontal or

radial component of the support reaction can significantly reduce the critical tensile ligament stress but tends to increase the other critical stresses. A reduction of the critical ligament stresses through an increase in the closure depth becomes ineffective once the closure depth/diameter ratio reaches ~ 0.7 . Accurate estimates of the perforated zone radius and the ligament efficiency are important for reliable results. The design of a closure capable of supporting an operating pressure of 11 MPa (1595 psi) is considered feasible on the basis of this analysis.

The two-dimensional axisymmetric analysis of the core cavity closure requires "smeared" properties to model the perforated central region and a special procedure for internal pressure loading. The axisymmetric analysis provides estimates of ligament stresses, but no indication of stress distribution in the ligaments is given. Therefore, a two-dimensional plane stress analysis has been performed for the core cavity closure to define more clearly the state of stress on a horizontal plane of the closure and to help formulate the procedure for analysis of internal pressure. The preliminary results of this analysis indicate that although two-dimensional axisymmetric analysis does not reveal the nature of stress distribution in the concrete ligaments, it can produce useful average ligament stress values for design purposes, provided accurate values for smeared properties are used.

A preliminary three-dimensional stress analysis of the core cavity closure for the demonstration plant was completed using the THREED computer program (Ref. 1-9). The cavity pressure on the bottom and outer cylindrical surfaces and within the penetrations was 10.2 MPa (1480 psi). Figure 1-11 presents a three-dimensional view of the model, showing the coordinate system and the arrangement of the elements. The 30-deg model corresponds to a closure with fifty-five 0.343-m (13.5-in.) diameter penetrations on a 0.505-m (19.87-in.) triangular pitch. Membrane elements with a 13-mm (0.5-in.) thickness simulate the penetration liners. The radius of the model is 2.54 m (100 in.), and the height 2.61 m (103 in.). These dimensions correspond to those used by Oak Ridge National Laboratory (ORNL) to

Fig. 1-11. THREED model for demonstration plant main closure

develop the first test model of the central cavity closure. The analytical model contains 651 nodes, eighty 20-node brick elements and 62 membrane elements. The nodes on the planes of symmetry at $\Theta = 0$ and 30 deg were constrained so that no tangential displacement was allowed. The nodes on the upper outer edge of the model were constrained in the axial direction so that they were a simple representation of a holddown system. This method of restraint does not simulate the effects of the steel shear anchors or the radial components of restraint which occur with a toggle holddown system.

The preliminary three-dimensional analysis results were compared with those for the two-dimensional analysis performed on the identical closure model and were found to differ by as much as 15%. The causes of these differences are being investigated. Figures 1-12 and 1-13 show the maximum and minimum principal stress contours, respectively, for the concrete ligaments in the Y-Z plane. The tensile stress approaches 20.7 MPa (3000 psi) at the top of the outer ligament, and the compressive stress 26.2 MPa (3800 psi) at the bottom of the innermost ligament. The compressive stresses are within the allowable values of ASME Section III, Division 2, provided that 55.2 MPa (8000 psi) concrete is used. The tensile stresses in the upper regions of the closure are unacceptably high for a linear analysis. Based on past two-dimensional analysis results, it is expected that including the effects of the steel shear anchors and the radial component of the toggle restraint system in the three-dimensional model will result in acceptable stresses in the upper regions of the closure. However, the differences between the two-dimensional axisymmetric and three-dimensional analytical results should be resolved before the three-dimensional model is revised.

The results of the three-dimensional, two-dimensional axisymmetric, and two-dimensional plane stress analyses of the core cavity closure for the commercial and/or demonstration plants are being compared. It is expected that this will result in a reliable and efficient procedure suitable for preliminary analysis and/or parametric study of the core cavity closure.

Fig. 1-12. Maximum principal stress contours for Y-Z plane under combined loading

Fig. 1-13. Minimum principal stress contours for Y-Z plane under combined loading

1.11.4. Thermal Barrier Studies

Development of the thermal barrier is currently directed toward establishing a materials data base and performing preliminary tests to explore the practicability and feasibility of floodable thermal barriers. Therefore, work is being done on the following four tasks: (1) determination of the effects of water exposure on ceramic materials; (2) determination of the effects of water exposure on metals and coatings; (3) determination of the effects of helium coolant impurities on metals; and (4) performance of feasibility tests of flooded thermal barrier components and assemblies. Work on the second task is scheduled to start in 1980. The status of the remaining three tasks is described below.

1.11.4.1. Effect of Water Exposure on Ceramic Materials. The MgO material recommended for the lower core cavity crucible structure (proposed at that time for postaccident fuel containment) was included for water compatibility tests together with the candidate fibrous insulation materials, Kaowool (Babcock & Wilcox) and Saffil Al_2O_3 (Imperial Chemical). These tests measured the leachability of the fibers and the MgO, the change in the load-bearing capability of the fibrous insulation during soaking and drying out, and the strength change of candidate MgO materials after water soaking. The major results to date show a high dissolution of calcium and impurities from MgO materials, which caused the strength of an industrial MgO refractory containing high CaO to decrease 50% after it was soaked in water for 1000 h. High-purity, high-density MgO was relatively unaffected by the soaking except for some mild surface etching. The Kaowool and Saffil fibers have not significantly leached after 1000 h and will be monitored for another 4000 h.

The interim results from the first tests of fibrous insulation soaked under load and dried out under no load showed a decrease in the springback load of 20% for Saffil and 40% for Kaowool. Permanent changes in the thicknesses of both materials after drying were greater than in tests where the materials were compressed at the same loads for the same times in air. A

combination Kaowool and Saffil blanket soaked and dried under load had an ~20% decrease in its load-carrying capability.

1.11.4.2. Effect of Helium Coolant Impurities on Metals. Previous analyses indicated potential decarburization of candidate alloys for thermal barrier components exposed to gas-cooled fast breeder reactor (GCFR) helium coolant at design temperatures. Thus, the decarburization kinetics of candidate and alternate alloys under GCFR conditions is being investigated. The alloys are 2-1/4 Cr - 1 Mo, 316 stainless steel, carbon steel, Hastelloy X, Alloy 800H, and Alloy 800. The specimens will be exposed for 500, 2000, 5000, and 10,000 h, and the kinetic data generated will be extrapolated to 30 (or 40) yr.

Much of the effort of the past six months has been spent in setting up the once-through helium testing facility (Fig. 1-14) which simulates the GCFR primary coolant chemistry. Figure 1-15 shows the specimen assembly in the test retort. The corrosion tests are currently under way. The 500-h specimens were removed from the test retorts and are being evaluated (i.e., weight measurement and carbon, metallographic, and electron microprobe analyses). Oxidation and decarburization behavior is also being investigated.

1.11.4.3. Feasibility Tests of Flooded Thermal Barrier Components and Assemblies. The exact conditions and duration of flooding and draining of the GCFR core cavity are not yet known, but the major concerns to be resolved for the thermal barrier include

1. Temporary loss or permanent deterioration of insulation blanket resiliency.
2. Fiber and blanket thickness shrinkage.
3. Fiber corrosion.

Fig. 1-14. Simplified flow diagram for once-through helium testing facility

Fig. 1-15. Specimen assembly in test retort (for helium coolant impurity effects)

4. Galvanic corrosion and stress corrosion of metallic components.
5. Possible shifting or dislocation of seals and blanket assemblies during flooding or draining.
6. Method, time span, and residual effects of dry-out on materials and interfaces.

A two-phase program for assessing the effects of flooding and developing a floodable thermal barrier has been initiated. The technical requirements for the engineering tests in phase 1 have been determined, the test housings designed and constructed, and testing started. The general objective of these tests is to determine the feasibility and practicability of flooding, draining, and drying a thermal barrier of the current fibrous insulation reference design. This experimental test program involves three different tests whose objectives and status are described below.

1. Absorption, draining, and retention tests. These tests were designed to establish the wetting, saturation, and drainage characteristics of a vertical column of compressed fibrous insulation, simulating the installed insulation component of a typical reactor cavity vertical wall thermal barrier. The test setup consisted of vertical strips of insulation (either Kaowool, Saffil, or a Kaowool/Saffil combination) compressed in a plexiglas enclosure. These tests have been completed, and the results are consistent, with no apparent slumping or shifting of the insulation blankets.
2. Insulation and mechanical stability test. The purpose of this test is to observe the effects of flooding and draining on the dimensional and mechanical stability of the thermal barrier assemblies. The test rig is a complete, multiple panel, full-size model of a typical vertical wall thermal barrier installation comprising cover plates, seal sheets, blanket support

angles, and simulated attachment fixtures which compress the insulation blankets inside a transparent housing. The test rig is designed to be easily disassembled for modification or replacement of parts that detract from floodability performance. Blanket compression can also be adjusted if necessary. The progression of water into and through the assembly will be followed by using colored water. Possible movement or deformation of the blankets or seals will be observed through the transparent sides of the housing. The test rig is presently in the set-up and installation phase, and fabrication of the housing and supporting framework is complete. All component parts have been manufactured and delivered, and installation of the thermal barrier and the first test of a representative Class A (Kaowool) assembly was begun.

3. Insulation drying tests. The primary purpose of these tests is to compare two alternative methods of drying out the encapsulated insulation blankets. The first method is heating to 204°C (400°F); the alternative method is heating to 66°C (150°F) (the maximum allowable mean temperature of the carbon steel vessel liner) and evacuating to 6.67 kPa (0.97 psi). The drying test assembly represents the intersection of four quarter-panels of a typical full-scale thermal barrier installation. Stainless steel is employed for all parts except the insulation. Drying rates are established by recording changes of weight. Kaowool and low-density Saffil are tested separately and in combination, making a total series of six tests. Four of these tests have been completed.

At this time, only the insulation absorption and drainage tests have been completed. Table 1-2 gives the test results. The results show good consistency, and although the Saffil insulation drains more rapidly than the Kaowool, there is remarkably little difference in the percentage of water (-40%) retained in the insulation after draining.

TABLE 1-2
WATER ABSORPTION AND DRAINAGE TESTS FOR THE
FLOODABLE THERMAL BARRIER

	Test No. 1 (Kaowool)	Test No. 2 (Saffil)	Test No. 3 (Kaowool/Saffil)
Volume of compressed insulation [m^3 (ft^3)]	0.0147 (0.52)	0.0147 (0.52)	0.0195 (0.69)
Weight of dry insulation [kg (lb)]	2.40 (5.3)	1.62 (3.58)	2.68 (5.92)
Initial weight of water (saturated insulation) [kg (lb)]	13.92 (30.7)	14.06 (31.0)	18.28 (40.3)
Water retained after draining [kg (lb)]	5.90 (13.0)	5.71 (12.6)	7.17 (15.8)
Percent of water drained	57.7	59.4	60.8

The drying tests are two-thirds complete, but no analyses of the results have yet been made. However, based on the absorption, draining, and drying tests made up to this point, there do not appear to be any drawbacks to discredit the feasibility of flooding.

The following generally encouraging observations were made:

1. The rate and degree of capillary rise (wicking) in the insulation was limited and of little significance to floodable thermal barrier design.
2. There was no noticeable slumping or displacement of insulation during flooding or draining.
3. About 60% of the water in the thermal barrier can be drained out in a few hours.

1.11.4.4. Conceptual Design Studies. Meaningful design development of a floodable thermal barrier is to a large degree contingent on the results obtained from the supporting R&D program. One of the early laboratory tests which measured changes in the resiliency of compressed submerged insulation showed a significant loss of the load-carrying capability of Kaowool immediately after draining off of the standing water. It had been recognized that this would probably be the critical point when heavily saturated and weakened vertical wall insulation blankets would be in danger of slumping (depending on the degree of success of the restraint methods employed).

In all the preliminary design proposals and studies of a flooded thermal barrier, maintenance of the static stability of all components of the vertical wall thermal barrier during flooding and draining was one of the major design problems. Much of the recent design effort has therefore been directed toward developing practical solutions to the problem of maintaining mechanical stability throughout flooding, draining, and dry-out. A

design presently being analyzed would remain stable even with complete temporary loss of insulation resiliency during or subsequent to flooding. The main feature of this design is the addition of a double support system of closely spaced, slender, sheet metal prongs which penetrate the blanket assembly from each side, providing stiffness and resistance to slumping by acting like teeth which firmly secure the wet blanket assembly. In addition, the retainer assembly adjacent to the liner incorporates an angled surface which provides secondary vertical support of the upper adjacent blanket assembly. This angled support surface has holes at the base for drainage.

1.12. REACTIVITY CONTROL AND INSTRUMENTATION MECHANISMS

The GA and Westinghouse design requirements for the control rod design mechanism have been integrated. Four design areas were examined in an effort to improve the control rod drive mechanism/shutdown rod drive mechanism (CRDM/SRDM) concept:

1. Key CRDM/SRDM dimensional requirements as a function of rod stroke and upper plenum height were examined.
2. A straightforward primary closure system for the CRDM/SRDM housings was developed. This system provides for external disconnection in air of electrical services to the drive and appears useful for remote, pressurized removal of the CRDM/SRDM from the PCRV.
3. A combination rod drive/instrument tree system supported from the control assembly was developed. It shows promise for simplifying rod connection and disconnection and provides more reliable rod/core positioning and readout independent of core growth.
4. A remote rod coupling/uncoupling system combined with an anti-rod-ejection system was developed.

1.13. FUEL HANDLING SYSTEM

Because there is a possibility of having to return (or maintain) reactor pressure to a level where natural convection occurs, screening studies were performed to determine the problems and effects of alternative pressurized shutdown conditions during

1. Repressurization from subatmospheric pressure to that required for natural convection [1.03 GPa (150 psig)], assuming equipment is installed in the PCRv. A "state of readiness" mode was considered in which 2 to 5 h are allowed before repressurization must take place.
2. Performance of all functions at natural convection pressure.

Preparation of refueling penetrations prior to refueling (installation of isolation valves) and removal of a circulator for maintenance present the most problems.

A preliminary assessment has been made of the problems which occur when the refueling concept adopted for the demonstration plant is applied to a six-loop, 1200-MW(e) plant. This particular concept is based on refueling seven assembly regions with an offset fuel handling machine through individual top head penetrations located over the center of each region. Three general parameters were considered:

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

To establish a base for subsequent parametric studies, a refueling time estimate was prepared based on the use of one-single assembly fuel transfer cask with a single fuel transfer chute connecting the cask to the storage pool (Fig. 1-16). Variations of this arrangement were studied in an attempt to reduce the refueling time significantly. Figure 1-17 depicts

Fig. 1-16. Refueling for the upflow core

Fig. 1-17. Refueling system for the upflow core

a concept featuring a fixed cask similar to that used in the Phenix 1 and 2 plants. The cask incorporates one or more spent fuel transfer locations as well as a new fuel loading location, which allows installation of new fuel into the fuel transfer cask almost immediately after removal of spent fuel. This coupled with the requirement for less vertical and lateral movement of the cask results in 15% to 20% less refueling time. An initial study on interfacing this transfer cask arrangement with the BOP has been done, and no significant problems are apparent at this time. A 10% reduction in refueling time may be had by refueling an entire region at one time instead of only two assemblies from each region. All time studies have been performed on this basis.

Figures 1-18 through 1-21 are preliminary conceptual arrangements for the fuel handling machine, the fuel transfer cask and grapple, and the fuel transfer cask support structure. Although these drawings are conceptual in nature and thus lacking in detail, they do reflect the viability and problems of the arrangement. There is the potential for problems in the fuel transfer cask. [A rough estimate for the cask weight is 158,757 kg (175 tons), with a center of gravity 5.8 m (19 ft) above the top of the PCR.V.] Although no analysis has been attempted as yet, it is evident that the loads induced under seismic conditions will be extremely high. To alleviate the effects of such loads, the supporting structure for the cask must be broader at the base, thereby obstructing access to more penetrations. If penetrations cannot be prepared in parallel with refueling operations, a significant loss of time will result.

1.17. REACTOR INTERNALS SYSTEM

1.17.1. Core Support Structure

The purpose of this subtask is to assure the availability of the structural analysis methods and materials mechanical behavior required to assess the structural integrity of the GCFR core support structure under all anticipated operational and safety-related loading conditions. Work accomplished during the last quarter included determination of the diameter

Fig. 1-18. Fuel handling machine

- ① UPPER BALL VALVE
- ② LOWER BALL VALVE
- ③ BI-STEM FUEL HOIST
- ④ FUEL TRANSFER CASK GRAPPLE
- ⑤ GUIDE SLEEVE
- ⑥ FUEL ASSEMBLY
- ⑦ HEAT EXCHANGER
- ⑧ CABLE REEL
- ⑨ FUEL ASSEMBLY SUPPORT (2 PLACES)

Fig. 1-19. Fuel transfer cask

Fig. 1-20. Fuel transfer cask grapple

Fig. 1-21. Fuel transfer cask grapple

and thickness of the grid plate for the two-loop demonstration plant. During this quarter a preliminary thermal analysis was performed on the core support structure to estimate bypass flow cooling requirements. A preliminary stress analysis was also performed using the thermal analysis results.

1.17.1.1. Thermal Analysis. The outer 50.8 mm (20 in.) of the grid plate were analyzed along with the grid plate support structure and the liner thermal sleeve. Two cases were studied: zero and 1% bypass flow (Figs. 1-22 and 1-23). With zero bypass flow, temperatures at the periphery of the grid plate and at other locations are excessively high. Temperatures are reduced to acceptable levels with 1% bypass flow.

1.17.1.2. Stress Analysis. The preliminary stress analysis included the effects of the temperature distribution due to 1% bypass flow. The results indicate that the conceptual design configuration analyzed (Figs. 1-22 and 1-23) satisfies the allowable stress values specified by the ASME Boiler and Pressure Vessel Code.

1.17.2. Reactor Shielding Assemblies

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

Work accomplished during the last quarter included a preliminary thermal analysis of the upflow radial shielding. During this quarter, the preliminary thermal analysis was continued, and several new concepts for upper and lower plenum shielding were developed.

Fig. 1-22. Zero bypass flow

Fig. 1-23. 1% bypass flow

The outer radial shield temperatures appear to be considerably lower than predicted in previous analyses. The primary reason for this is the reduction predicted in nuclear heating rates and grid-plate-mounted shield temperatures. When 1% core coolant bypass flow is used to cool the radial shields, their maximum temperature is in the range of 370°C (700°F); with zero bypass flow, the maximum temperature is in the range of 450°C (850°F). More accurate specification of bypass flow cooling requirements will depend on more complete analyses of the shielding assembly and the core support structure.

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 performance and reliability by testing under anticipated operating conditions. The overall objective for FY 79 was to continue conceptual design and performance analyses of the reference design circulator and support the alternate design studies for an upflow core. The compressor assembly, service system, and loop isolation valve were designed. Test rigs were designed for two purposes: (1) testing the bearings and bearing water supply system and (2) testing a scale-model compressor and loop isolation valve.

1.21.1.1. Main Circulator Design. The conceptual design of one version of a main helium circulator for a two-loop 400-MW(e) demonstration plant was completed for a horizontal circulator installation in the bottom of the PCRV. This circulator concept would be common to the commercial and the demonstration plant and was used as the basis for the scale-model circulator design in Ref. 1-10 and the bearing test rig design. The design conditions were based on a 30,000-hp motor at 3,000 rpm.

1.21.1.2. Main Circulator Aerodynamics. A "pipe" diffuser was designed for the 3000-rpm version of the main circulator (the rotor is described in Ref. 1-10). The objective of the design was to provide an overall o.d. of

less than 2.85 m (112.25 in.), which is the maximum allowable envelope diameter of the circulator cavity in the PCRV. A choice of 13 passages, with the divergence angle increased from the optimum of 8 to 10 deg, produced an o.d. around the exit ducts of 2.80 m (110.25 in.), which is within the required maximum. The resulting design is shown in Fig. 1-24. Reference 1-11 summarizes the development of the current aerodynamic design.

The off-design performance of the circulator compressor was calculated for operation with the reactor at design point pressure. Because the compressor rotor is of the "squirrel cage" type with rotor blades extending through only the radial portion of the rotor passage rather than the inlet plane, it cannot be analyzed with the existing PREDM computer program (Ref. 1-12). The performance characteristics were therefore derived from a non-dimensional plot of pressure rise coefficient against flow coefficient calculated for a compressor of roughly the same size, equipped with a vaned diffuser.

The efficiency was calculated from pressure rise for each constant-speed characteristic using the equation

$$\eta = \frac{\Delta P / \rho}{\sigma U^2 / g},$$

where ΔP = pressure rise,

ρ = gas inlet density,

U = impeller tip speed,

σ = "slip factor,"

g = mass acceleration conversion factor.

The slip factor was assumed to be constant with mass flow rate and rotor speed and equal to the design point value inherent in the design charts of Ref. 1-13. An arbitrary reduction in peak efficiency for each speed was built in by assuming that the peak efficiency dropped by approximately eight percentage points as speed changed from 100% to 33% of design speed.

Fig. 1-24. Main circulator diffuser for 3000-rpm impeller

Figure 1-25 shows the off-design performance at fully pressurized reactor conditions. Pressure rise is plotted versus mass flow rate for constant circulator rpm at speeds from 20% to 120% of design speed, and curves of constant compressor efficiency are superimposed on the constant-speed curves.

1.21.1.3. Bearing Test Rig. Final design and analysis of the bearing test rig (Fig. 1-26) was completed. The test rig provides for a full-scale test of the GCFR main circulator water bearings, shaft-mounted pump, and external service system. The unit is driven by a variable-speed electric motor, the shaft is rigidly held in space with ball bearings, and the free-floating water bearing housing is loaded with hydraulic jacks. The jack loads and resulting housing displacements will be measured to provide bearing spring rates for various conditions.

The bearing cartridge is supported on two sets of partial-arc hydrostatic oil bearings. This support arrangement provides almost frictionless transmission of the radial bearing loads from the loading jacks to the bearing housing. The housing reaction torque from the shaft-mounted pump and the bearing drag is measured with a torque arm and load cell. Each partial-arc pad is approximately 203 mm (8.0 in.) wide and 560 mm (22.0 in.) long. The pad is composed of four pockets; each pocket is 64 x 240 mm (2.56 x 9.4 in.). The maximum design load of 53.3 kN (12,000 lbf) is supported with an oil pressure of 17.6 kPa (121 psig), and there is a film clearance of 0.05 mm (0.02 in.). The oil flow for each partial-arc bearing is 0.83 liters/min (0.2 gpm), and the supply to each pocket provides an additional 17.6-kPa (121-psig) pressure drop.

The shaft support bearings are two identical sets of extra light series duplex bearings with a 180-mm (7.1-in.) bore, a 280-mm (11.0-in.) o.d., and a 46-mm (1.8-in.) width. The bearings are mounted in a face-to-face configuration with a light preload. The motor end is a floating fit in the housing, and the opposite end is fixed. Bearing life calculations were carried out in accordance with the manufacturer's catalog using a design load of 53.3 kN (7000 lb) and a speed of 3000 rpm. The resulting

Fig. 1-25. Main circulator off-design performance

Fig. 1-26. Bearing test rig

B-10 life is 3300 h, assuming that one bearing carries the full load. In addition, the ball bearing frictional losses were calculated using the maximum load and speed on a single bearing. Two methods were used, and the results were averaged to give a power loss of 1.74 kW. Further calculations were made to determine the required oil flow for a jet lubrication system (the lubricant is Gulf Harmony 47). Oil is supplied to a single jet aimed at the inner bearing race. The oil pressure is ~2.18 KPa (15 psig) to provide an oil velocity of 15.24 m/s (50 ft/s). Each bearing is supplied with 2.20 liters/min (0.50 gpm) of oil, which removes the frictional heat with a temperature rise of 14.4°C (58°F). The oil jet orifice is 1.52 mm (0.06 in.) in diameter, and the oil drain hole for each bearing is 15.88 mm (0.625 in.) in diameter. This provides a net drain area of 200 mm² (0.31 in.²) and a drain velocity of less than 0.61 m/s (2 ft/s), thus assuring that the oil does not build up in the bearing housings.

A series of labyrinths is provided at different points along the shaft to help prevent the escape of oil and water (see Fig. 1-26). The flow rates for the labyrinths were calculated using the LBSEAL computer program (Ref. 1-14).

The radial clearance for the water bearing labyrinth is 0.15 mm (0.006 in.), with a ΔP of 690 Pa (0.1 psi) and a flow of 2.5 liters/s (5.3 cfm) per labyrinth. The radial clearance for the oil bearing labyrinth is 0.08 mm (0.003 in.), with a ΔP of 690 Pa (0.1 psi) and a flow of 1.05 liters/s (2.23 cfm) per labyrinth.

1.21.1.4. Rotor Dynamics, Shaft and Bearings. An effort was made to reduce the impeller overhang and weight, resulting in a redesigned impeller with a 1.31-m (51.75-in.) diameter and a weight of 747 kg (1650 lb). The seals were redesigned, which reduced the overhang to 0.29 m (11.31 in.). The resulting configuration has an inboard bearing stiffness of 5.25×10^8 N/m (3.0×10^6 lb/in.) or more (Fig. 1-27).

Fig. 1-27. Required bearing stiffness with reduced impeller overhang and 254-mm-diameter bearing

1.21.1.5. Main Loop Isolation Valve. Figure 1-28 shows the model isolation valve developed for the 1/3-scale model test rig. This valve model will be tested together with the 1/3-scale model circulator in the air flow test rig described in Ref. 1-10.

1.21.2. Steam Generator

The function of the steam generator is to transfer heat from the reactor primary coolant (helium) to the secondary coolant (water/steam) during normal plant operation. The steam generator will be subect 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 for the upflow core study features a top-exhaust arrangement. Owing to concerns about the expansion loop design in a bottom-supported, top-exhaust arrangement, efforts were made to develop a bottom-exhaust, bottom-supported steam generator for an upflow core. A top-supported, top-exhaust arrangement was also investigated.

The bottom-supported, bottom-exhaust arrangement was made possible by placing the CAHE in the same cavity as the steam generator. The major impact of this arrangement was evaluated (Ref. 1-15) and the following conclusions reached:

1. The radial flow auxiliary circulator must be replaced by an axial flow circulator to eliminate a potential cold trap during natural convection startup.
2. The PCRV diameter must be increased from 39 m (130 ft) with a top-exhaust steam generator arrangement to 41 mm (136 ft) with a bottom-exhaust arrangement.

Fig. 1-28. Main loop horizontal valve for 1/3-scale test rig

3. The PCRV ducting is more complicated and the tendon layout more difficult.
4. The steam generator expansion loop design risk is greatly reduced with a bottom-exhaust arrangement.
5. BOP safety class piping separation is improved when the main steam pipe is located at the bottom of the PCRV.
6. The steam pipe restraint design is less complex for a bottom-exhaust steam generator.
7. The overall cost increase is a small fraction of the total plant cost.

Despite the many advantages of the bottom-exhaust arrangement, the lack of a self-isolation capability during natural convection, when the main loop isolation valve fails to close, still makes a top-exhaust steam generator preferable. To alleviate the expansion loop thermal stress, a new concept with top-supported design was investigated.

Figure 1-29 shows the top-supported design. Water enters through a side-penetration feedwater tube sheet and boils in the helical bundle. The superheated steam is then exhausted from the top of the PCRV. The main bundle support is attached to the liner at the top of the bundle. Evaluation of this design indicates that a redundant support should be provided as a backup to the primary support member which is in the high-temperature end of the unit. A seismic restraint is also required to reduce the seismic stress (Ref. 1-16). Because the bundle support is located at the top, the hot helium flow must be sealed at the bottom of the bundle. The design of a large hot seal having minimum bypass will be challenging. In addition to the anticipated difficulties with the seal design and the seismic restraint requirement, subsection NF of Section III of the ASME Code does not include evaluated temperature rules. However, a code case to include a support structure at elevated temperature is expected in the near future.

Fig. 1-29. Top-supported, top exhaust steam generator

The top-supported bundle considerably reduces the problems with the top-expansion loop design. However, the lower expansion loop for the feed-water lead-in tubing is exposed to large thermal stress, and the final design must be capable of absorbing 7.1 mm (2.8 in.) of thermal expansion. It must also satisfy natural frequency requirements to avoid resonance with the main bundle. A more detailed study of this problem is being carried out.

Other tasks were accomplished in support of a commercial size helical bundle design. Preliminary design curves for the expansion loop have been developed (Ref. 1-17). Figure 1-30 shows a range for allowable expansion loop size and the corresponding natural frequency and stress values caused by the differential thermal expansion between the superheat straight tube and the tube bundle. The design will accommodate thermal stress as well as natural frequency, which must be higher than the fundamental frequency in the bundle to avoid resonance phenomena.

An analysis was made to determine the number of support plates required to prevent flow-induced vibration damage to tubes in a helical bundle (Ref. 1-18). The flow of helium over a helical tube bundle can induce large-amplitude tube vibrations once the helium flow velocity exceeds a critical velocity. The resultant vibrations are called whirling vibrations because the tube vibrates in large-amplitude oval orbits, resulting in commercial heat exchanger tube failures. The tubes typically fail because of either fatigue cracks at a tube sheet, fretting wear of the tubes at the support plates, or flats wearing into adjacent tubes where they clash at midspan. Unstable whirling vibrations can be prevented by ensuring that the helium flow velocity does not exceed the critical velocity. Since critical flow velocity increases as the number of tube support plates increases, the simplest method of preventing whirling in a helical bundle is to increase the number of tube support plates until the critical velocity is raised above the helium flow velocity.

Fig. 1-30: Steam generator expansion loop design

A computer program was developed to calculate critical velocity as a function of number of support plates. This program is being used with other programs to provide conceptual design information for the helical bundle. A tube count computer program was also developed to furnish data on the maximum number of tubes which can be allocated to a helical tube bundle. Information on the minimum inner shroud diameter, frontal area, and void fraction (percentage of area between tube rows) is also provided. These data are used as input for sizing the steam generator and can also be used to determine the number of tube rows and tubes per row.

The tube count program consists of subroutines used for different helical coil configurations: (1) steam generator helical coils having all full support plates (those reaching from the outer to inner shrouds) and (2) coils having one-half full support plates and one-half partial plates. There are separate sets of data for coils which have straight tubes passing through the inner shroud, such as the bottom-exhaust arrangement, and for those in which no tubes pass through the center of the coils.

Various concepts for thermal insulation of the superheat tube sheet were developed. The tube sheet dimensions were calculated based on ligament efficiency and minimum space requirement for the tube orbital welder (Ref. 1-19). The objective was to find alternative methods of providing thermal insulation of the superheat tube sheet during a transient and using 2-1/4 Cr - 1 Mo for the tube sheet and the barrel section, thus eliminating severe transients. Further study is needed to reduce the superheat tube sheet to a practical configuration.

Reference 1-20 summarizes the results of the alternate materials study for the steam generator bundle. The materials considered were 12 Cr, Super 9 Cr, 9 Cr, Alloy 800, and decarburized 2-1/4 Cr - 1 Mo. The purpose of the study was to provide an alternative to eliminating the bimetallic weld.

An acoustic test rig was designed and fabrication begun. Preliminary data will be obtained on acoustic resonance due to vortex shedding in a helical bundle. The intense acoustic resonance in the GCFR steam generator

was evaluated (Ref. 1-21). The test program is part of the study of such phenomena. Funding is not available to continue the study at this time.

1.23. HELIUM SERVICE SYSTEM

The GCFR core consists of metal-clad fuel assemblies vented directly to the helium purification system (HPS). Compared with the prismatic fuel used in a high-temperature gas-cooled reactor (HTGR), vented fuel results in increased fission product transfer to the HPS, particularly during plant depressurization. This imposes a significantly increased activity inventory and decay heat generation within the HPS, requiring the HPS design previously considered for GCFR application (similar to that for a prismatic fuel HTGR) to be significantly modified.

A study was performed to determine the HPS fission product activity and decay heat generation rates associated with GCFR vented fuel and to develop an updated HPS design to accommodate the imposed requirements. Figure 1-31 shows the flow sequence of the proposed purification system. This design uses the generic HPS processing scheme, but with additional delay beds. Both water-cooled and freon-cooled charcoal delay beds are needed to handle the extremely high decay heat loads from the noble gases vented from the fuel rods during plant depressurization, either accidental or planned.

Figure 1-32 shows the noble gas trapping part of the HPS. All modules in the HPS require 1.65 m (5-1/2 ft) of concrete shielding. Although this design is specifically for a commercial size plant, there is not expected to be a significant difference in processing or sizing for a smaller demonstration plant.

Fig. 1-31. Simplified flow sketch of proposed HPS

WATER-COOLED ADSORBERS

454 KG (1000 LB) CHARCOAL EACH [0.9-M (3-FT) DIAMETER x 1.5-M (5-FT) T-T]

$Q = 57,150 \text{ W (195,000 BTU/H) MAXIMUM}$

WATER FLOW = $1.64 \text{ KG/S (3,000 LB/H) = } 0.002 \text{ M}^3/\text{S (26 GPM)}$

MODULE SIZE = $3 \times 3 \times 3 \text{ M (10} \times 10 \times 10 \text{ FT)}$

FREON-COOLED ADSORBERS

91 KG (200 LB) CHARCOAL EACH [0.5-M (1.5-FT) DIAMETER x 1.2-M (4-FT) T-T]

$Q = 58,600 \text{ W (200,000 BTU/H) MAXIMUM}$

REFRIGERANT FLOW = $9.44 \text{ KG/S (75,000 LB/H) = } 0.014 \text{ M}^3/\text{S (94 GPM)}$

MODULE SIZE = $3.0 \times 6.1 \times 4.3 \text{ M (10} \times 20 \times 14 \text{ FT)}$

LOW-TEMPERATURE ADSORBERS

459 KG (1000 LB) CHARCOAL EACH [0.9-M (3-FT) DIAMETER x 1.5-M (5-FT) T-T]

$Q = 58,600 \text{ W (200,000 BTU/H) MAXIMUM}$

$\text{LN}_2 \text{ STORAGE} = 8.4 \text{ M}^3 (2200 \text{ GAL})$

MODULE SIZE = $3.0 \times 6.1 \times 4.3 \text{ M (10} \times 20 \times 14 \text{ FT)}$

Fig. 1-32. Proposed noble gas trapping system

1.28. CORE AUXILIARY COOLING SYSTEM (CACS)

1.28.1. Auxiliary Circulator, Valve and Service System

1.28.1.1. Auxiliary Circulator Design. The auxiliary circulator is designed to function as part of the core auxiliary cooling system (CACS), circulating primary coolant for removal of residual heat from the core after the reactor has been shut down, including during refueling. The purpose of this task is to develop an auxiliary circulator to meet the performance, safety, and reliability criteria of the CACS.

Numerous changes in the overall plant design which could impact the design of the auxiliary circulator and auxiliary loop isolation valve are being considered. Two steam generator configurations, a top exhaust and a bottom exhaust, have been evaluated. The top-exhaust steam generator coincides with a bottom-mounted auxiliary circulator; the bottom-exhaust layout results in the auxiliary circulator being mounted in a top penetration, separate from the CAHE. In the latter arrangement, the penetration space required by the top-mounted auxiliary circulator could affect the overall size of the PCRV and thus have a substantial cost impact.

An auxiliary circulator design study based on current estimated system parameters indicated that the circulator diameter can be sufficiently reduced from the reference design to preclude any effect on PCRV diameter for either mounting arrangement. An axial flow compressor appears feasible for the top-mounted circulator, thus allowing a more favorable flow path for natural convection cooling.

Figure 1-33 shows a motor and bearing assembly layout for the bottom-mounted circulator. It is different from the top-mounted reference design arrangement in that it has a stronger outer casing, realigned bearing supports, inverted oil reservoirs, and a revised assembly sequence. During the next quarter, conceptual design work will begin on either a top- or bottom-mounted auxiliary circulator. The orientation chosen for the auxiliary circulator will depend upon which steam generator/PCRV arrangement has been selected.

Fig. 1-33. Auxiliary circulator motor assembly (bottom mount)

1.28.1.2. Auxiliary Loop Isolation Valve Design. 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 with the CACS or during natural convection cooling. The purpose of this task is to develop an auxiliary loop isolation valve to meet the performance, safety, and reliability criteria of the CACS.

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, compatible with natural convection cooling using the CACS. The location of the valve depends on the auxiliary circulator orientation. The valve will be mounted in either the inlet duct or the discharge duct of the auxiliary circulator and will likely be installed in a vertical duct.

Independent of the mounting location, some design decisions have been made for the auxiliary loop isolation valve:

1. The valve must be actuated by gravity to open and facilitate natural convection cooling using the CACS.
2. Counterweights must be used as necessary to bias operation and allow closing by using a nominal amount of aerodynamic force when the main loops are operating.
3. An actuator providing either continuous or intermittent force must be part of the design. This feature should include the ability to partially open the valve plates for test purposes.
4. A fluidic position indicator system for each valve plate must be included for both the open and close conditions.

The conceptual design phase for the auxiliary loop isolation valve will begin during the next quarter.

1.28.1.3. Auxiliary Circulator Service System Design. The auxiliary circulator service system performs four main functions:

1. It controls the flow of purge helium supplied to and returned from auxiliary circulators. This helium prevents primary coolant-to-motor bearing oil cross-contamination. Purge flow is not required for standby, startup, and operation of the auxiliary circulator and therefore has no safety function.
2. It provides part of the primary coolant pressure boundary because it is possible for primary coolant to flow along the auxiliary circulator shaft and into the auxiliary circulator service system piping.
3. It removes small quantities of oil vapor or mist from purge helium returning from auxiliary circulator motor cavities. Clean helium is returned to the purified helium compressors in the HPS. Since auxiliary circulator operation does not require purge helium flow, the oil adsorber section of the auxiliary circulator service system has no safety function other than as a primary coolant pressure boundary.
4. It provides the piping and components necessary for replacing the oil in the auxiliary circulator bearing oil reservoirs. Since these maintenance operations are performed while the auxiliary circulator is shut down and not available for service, the oil change equipment has no safety function other than as a primary coolant pressure boundary.

Auxiliary circulator service system equipment in previous designs has been located in a total of five modules per plant: one bearing oil module (oil cart), three service modules, and one oil adsorber module. Because

primary coolant system boundary isolation valves are located as close as possible to auxiliary circulators, all auxiliary circulator service system modular equipment is nonsafety related. Without requirements for loop separation of modular equipment, all nonsafety equipment could be located in one service module which would serve all three circulators. Because of the potential reduction in design, fabrication, installation, and building space costs, a new auxiliary circulator service system design with all nonsafety equipment consolidated into one service module is being evaluated.

1.28.2. Core Auxiliary Heat Exchanger (CAHE)

The heat exchanger for core auxiliary cooling in an upflow core must be designed to perform under natural convection conditions in both helium and water loops. To satisfy natural convection requirements, the CAHE must be placed at a specified elevation above the core. Two basic CAHE designs, compact bayonet and helical, were evaluated. The bayonet design was ruled out because of the current uncertainty about flow and thermal performance characteristics under natural convection conditions (Ref. 1-22). Therefore, the helical bundle was selected as the reference configuration, and considerable effort was made to develop a design with minimum tubing stress. The CAHE is placed in the same cavity with the steam generator, which must have a bottom-exhaust arrangement. In this configuration, the CAHE helical bundle has a smaller ratio of outer bundle diameter to inner bundle diameter. The stress is reduced to a level at which support plates can be welded to the inner shroud without introducing excessive bear-hug stress on the tubes (Refs. 1-23, 1-24). However, if a top-exhaust arrangement is used, the CAHE cannot be placed above the steam generator in the same cavity, but must be supported in its own cavity, as shown in Fig. 1-34. Detailed stress calculations will be performed in the conceptual design phase.

Studies on the CACS were carried out to evaluate the feasibility and advantages of a drum-boiler system. In this system, water enters the CAHE at saturation temperature and exits as low-quality steam. The steam is then routed outside the PCRV to a drum which separates the vapor from the

Fig. 1-34. CAHE helical bundle

water. The vapor rises 21 m (70 ft) above the drum to an air cooler in which the vapor is condensed and returns with the water in the drum to the CAHE inlet. The water-side circulation is completely driven by density difference and is self-regulated as load changes.

A preliminary evaluation of the helical bundle CAHE design in a drum-boiler system determined the following:

1. CAHE performance is not limited by the minimum circulation ratio (or CAHE exit steam quality), because in a helical bundle, steam blanketing and boiling instability, which are common in conventional boilers, are greatly reduced.
2. The helical bundle bear-hug stress generated by the difference between the thermal expansions of the plates and the tubes is reduced because of the smaller temperature difference between the helium and the boiling water.
3. A major difficulty with the all-liquid CAHE design is that the tube sheets are subject to severe transients during cooldown. In a drum-boiler CAHE, the water stays practically constant at saturation temperature throughout entire transients, thus eliminating thermal shocks on the tube sheets and tube sheet assembly.
4. The present tubing reference material is 2-1/4 Cr - 1 Mo for either the all-liquid or the drum-boiler system. Alloy 800H might be ruled out in a drum-boiler system because of stress corrosion in a two-phase regime.

A preliminary analysis with combined performance and structural equations for the all-liquid system has been developed for use in conceptual design (Ref. 1-25). The analysis is applicable to helical coil bundle selection for steady-state operation.

1.32. PLANT PROTECTION SYSTEM

Various protection systems and preliminary logic configurations and trip parameters have been examined. Secondary cooling system and CACS initiation logic were also generated.

1.33. PLANT CONTROL SYSTEM

1.33.1. Microcomputer Control

A development program to install, check out, program, and evaluate an LSI-11/2 microcomputer was performed. This program was applied to plant control and status monitoring of plant equipment. After installation of the microcomputer, memory, and serial interface, diagnostic programs were executed and confirmed that all the functions of the components were working properly. Assembling, editing, and linking were performed.

The process input/output modules were added, and each of the three modules was subjected to comprehensive tests. Diagnostic programs were executed to confirm the correct operation of the input/output modules. Then a servomotor was controlled from an analog signal supplied by the D/A converter module, and the speed of the servomotor was monitored by the A/D converter module. This experiment confirmed the suitability of the microcomputer system for this type of control application. Reference 1-26 describes the microcomputer control development program.

1.33.2. Residual Heat Removal

The conceptual synthesis of CACS and secondary cooling system residual heat removal (RHR) control instrumentation has been completed.

1.33.3. System Description

A system description for the plant control system was issued (Ref. 1-27).

1.35. DATA HANDLING SYSTEM

The operation of the prototype data acquisition, processing, and display system was continued to accrue operational, reliability, and maintainability data for use in planning and conceptual design of the data handling system.

1.36. ANALYTICAL INSTRUMENTATION

There was no activity on this task during this quarter.

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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.

2.1.1.2. Cladding Shield and Structural Materials Technology. There was no activity on this task during this quarter.

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 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, etc.) 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 in this environment.

Oxidation test OX-2 has reached an exposure of ~4400 h, with a goal exposure of 5000 h. The test conditions are as follows:

Partial pressure of H_2	~100 Pa
Partial pressure of H_2O	~400 Pa
Temperature	600° and 700°C

Both iron and chromium oxides are stable in this environment compared with test OX-1, in which only chromium oxide is stable. Consequently, the oxidation behavior of the cladding might be expected to be different in this test. An interim examination after 2500 h of exposure was completed, and the oxides on all the test specimens, excluding as-received smooth and electrochemically etched 316 stainless steel, appeared to be protective. The as-received smooth and electrochemically etched specimens showed minimum oxide spallation.

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 technique, 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 were fabricated. ANL has also assembled a test rig for testing cladding specimens where the hoop and axial tensile stresses are equal.

2.1.1.2.1.3. PNL Helium Loop Test Program. Biaxial creep rupture tests are being performed in a recirculating helium loop at PNL. These tests are similar to those being performed at ANL in a quasistatic environment. The results of both tests will be compared to determine whether mass transfer effects on the boundary layer may be present in the ANL tests. PNL has successfully operated the helium loop with controlled impurities and gained experience with the impurity injection system. The impurity monitoring system, i.e., the Thermox meter for measuring H_2/H_2O and the gas chromatograph for measuring H_2 , has operated satisfactorily, indicating an H_2/H_2O range of 7 to 15 for about one week. The original set of test specimens will be installed in the loop and tests continued until all specimens fail by rupture.

2.1.1.3. Trap Materials Technology. There was no activity on this task during this quarter.

2.1.1.4. Control Materials Technology. There was no activity on this task during this quarter.

2.1.1.5. Irradiation Testing.

2.1.1.5.1. F-1 Fast Flux Irradiation Experiment

The encapsulated fuel rods in the F-1 (X094B) experiment received burnup exposures in the range 8 to 13.6 at. % (8×10^{26} n/m² total fluence and 6.1×10^{24} n/m², $E > 0.1$ MeV). Postirradiation examination of the seven fuel rods removed at the termination of the experiment is continuing at ANL, and examination of special components (dosimetry and charcoal traps) is continuing at GA.

During this quarter, Idaho Nuclear Engineering Laboratory (INEL) completed mass spectrometric analysis of cesium and rubidium on residual charcoal trap samples from the F-1 rods. The fission product data have been pooled with previous data to develop complete trap concentration profiles for the various fission isotopes. Figures 2-1 through 2-8 show the fission product distribution profiles, and Tables 2-1 through 2-4 give the fission products loadings for each rod. In general, the isotopic loading (ppm) for each of the lower traps uniformly decreases about an order of magnitude for the isotopes Cs-133, Cs-135, Cs-134, and Cs-137. The sequence is approximately the same in the upper traps except that the Cs-137 level increased to above that for Cs-134. The concentration of Cs-137 in the lower traps is at least one order of magnitude lower than that in the respective upper traps. The Cs-137 concentration in the lower G-9 inlet trap section is about the same as the Cs-133 concentration (~10,000 ppm versus ~10 ppm Cs-137 in the midtrap section), which may be due to the enhanced mobility of cesium vapor from a low fuel oxygen/metal (O/M) ratio.

A method for determination of iodine on the F-1 traps was developed, but actual measurements on samples was deferred. Determination of radioiodine on charcoal is complicated because of the densification of the charcoal in these rods, i.e., 40% to 60% volume shrinkage. Dissolution of the charcoal required vigorous oxidation heating, which may have caused

Fig. 2-1. Isotopic concentration vs trap axial position for G-4 lower trap

Fig. 2-2. Isotopic concentration vs trap axial position for G-4 upper trap

Fig. 2-3. Isotopic concentration vs trap axial position for G-9 lower trap

Fig. 2-4. Isotopic concentration vs trap axial position for G-9 upper trap

Fig. 2-5. Isotopic concentration vs trap axial position for G-11 lower trap

Fig. 2-6. Isotopic concentration vs trap axial position for G-11 upper trap

Fig. 2-7. Isotopic concentration vs trap axial position for G-13 lower trap

Fig. 2-8. Isotopic concentration vs trap axial position for G-13 upper trap

TABLE 2-1
G-4 ROD FISSION TRAP ISOTOPIC INVENTORY(a,b)

Trap Section	Axial Position(c) (cm)	Cs-133	Cs-135	Cs-137	Rb-85	Rb-87
1	-19.8	7,267	1,220	1	1,990	501
2	-15.2	6,685	1,113	2	214	53
3	-10.0	5,247	1,019	3	1,755	531
4	-5.7	(d)	(d)	(d)	(d)	(c)
5	-3.0	809	84	4	151	56
6	-1.5	17,077	3,621	13	343	126
7	+0.63	19,773	13,800	4,368	660	640
8	+1.9	1,576	694	183	71	40
9	+4.57	1,474	529	76	328	145
10	+8.50	2,387	679	4	1,053	413
11	+13.7	3,003	764	3	137	48
12	+19.3	16,298	3,405	12	3,674	924
Trap inventory (μg)		88,020	27,656	4,672	10,496	3,520
Production inventory (μg)		464,900	463,500	452,100	49,220	92,130
Percent in trap		18.9	5.97	1.03	21.32	3.82

(a) Micrograms/section.

(b) Concentrations at the end of irradiation.

(c) Measured from end of trap nearest the fuel to trap section midpoint.

(d) Not determined, sample lost.

TABLE 2-2
G-9 ROD FISSION TRAP ISOTOPIC INVENTORY(a,b)

Trap Section	Axial Position(c) (cm)	Cs-133	Cs-135	Cs-137	Rb-85	Rb-87
1	-19.8	7,999	1,387	1	2,472	616
2	-15.2	3,527	663	3	123	43
3	-10.0	5,179	847	9	1,241	411
4	-5.7	2,042	350	9	82	33
5	-3.0	1,674	351	16	60	26
6	-1.5	6,408	4,293	3,172	730	901
7	+0.63	549	312	151	81	40
8	+1.9	815	390	144	40	25
9	+4.57	659	280	74	44	21
10	+8.50	1,849	881	359	710	413
11	+13.7	(d)	(d)	(d)	(d)	(d)
12	+19.3	6,482	2,235	386	2,059	959
Trap inventory (μg)		36,964	12,223	4,363	7,696	3,527
Production inventory (μg)		303,700	308,300	296,830	33,760	60,770
Percent in trap		12.2	3.96	1.47	22.8	5.85

(a) Micrograms/section.

(b) Concentrations at the end of irradiation.

(c) Measured from end of trap nearest the fuel to trap section midpoint.

(d) Not determined, sample lost.

TABLE 2-3
G-11 ROD FISSION TRAP ISOTOPIC INVENTORY(a,b)

Trap Section	Axial Position ^(c) (cm)	Cs-133	Cs-135	Cs-137	Rb-85	Rb-87
1	-19.8	8,166	1,335	--	2,208	578
2	-15.2	4,383	777	--	168	53
3	-10.0	9,212	805	3	1,130	360
4	-5.7	2,084	434	6	70	25
5	-3.0	1,350	2,932	6	56	21
6	-1.5	6,034	1,562	59	148	95
7	+0.63	4,056	2,310	100	139	150
8	+1.9	1,435	5,187	124	64	33
9	+4.57	2,706	1,049	337	77	36
10	+8.50	4,259	1,248	203	1,225	515
11	+13.7	5,180	1,295	165	253	71
12	+19.3	24,293	5,359	632	4,645	1,474
Trap inventory (μg)		58,599	24,293	1,635	10,183	3,411
Production inventory (μg)		323,600	330,800	319,700	35,180	65,770
Percent in trap		18.11	7.34	0.51	28.95	5.20

(a) Micrograms/section.

(b) Concentrations at the end of irradiation.

(c) Measured from end of trap nearest the fuel to trap section midpoint.

TABLE 2-4
G-13 ROD FISSION TRAP ISOTOPIC INVENTORY(a,b)

Trap Section	Axial Position(c) (cm)	Cs-133	Cs-135	Cs-137	Rb-85	Rb-87
1	-19.8	8,089	1,297	1	2,196	539
2	-15.2	6,030	1,096	--	156	51
3	-10.0	5,085	1,036	3	1,549	608
4	-5.7	1,592	348	3	62	27
5	-3.0	1,160	275	407	60	28
6	-1.5	4,400	1,305	183	134	94
7	+0.63	1,340	421	86	53	30
8	+1.9	1,463	365	45	68	25
9	+4.57	1,576	364	55	58	25
10	+8.50	7,753	1,747	172	1,006	374
11	+13.7	1,934	413	23	116	43
12	+19.3	5,850	1,193	117	1,884	724
Trap inventory (μg)		42,312	9,860	692	7,342	2,612
Production inventory (μg)		323,600	330,800	319,670	34,610	64,760
Percent in trap		13.08	2.98	0.22	21.21	4.03

(a) Micrograms/section.

(b) Concentrations at the end of irradiation.

(c) Measured from end of trap nearest the fuel to trap section midpoint.

loss of iodine. A modified procedure using synthetic samples has overcome this difficulty, but the method must be applied to the F-1 samples for confirmation.

2.1.1.5.2. GB-10 Vented Fuel Rod Experiment

A series of analyses planned to investigate the anomalous thermocouple readings taken during irradiation of GCFR vented sweep gas capsule GB-10. The following calculations are planned: (1) end of life (EOL) power distribution using the fissile atom distribution measured by electron and ion microprobe measurements made during destructive postirradiation examination (PIE), (2) EOL temperature distribution in the capsule (from the power distribution); (3) bowing of the fuel rod and its effect on temperature distribution, thermocouple readings, and corrections applied to thermocouple readings to obtain cladding and fuel temperatures. If the thermocouple readings can be understood, the uncertainty in cladding and fuel temperatures can be greatly decreased, and the confidence in the correlation of fission gas release diffusion parameters and temperature will be significantly improved (see Section 2.1.1.5.2.1). In addition, the relative distribution of cesium, rubidium, and iodine has been in the fuel blanket and trap regions of GB-10. As expected, high cesium loading was found (see Section 2.1.1.5.2.2).

2.1.1.5.2.1. GB-10 Neutronic Analysis. Capsule GB-10 was irradiated as one of a series of fuel rod irradiation tests conducted at the Oak Ridge Research Reactor (ORR) poolside facility. Neutronic analyses were performed on GB-10 at EOL. The purposes of these analyses are

1. To calculate the distribution of power generated within the GB-10 fuel rod at the end of the irradiation period. The results of this analysis will be used for analyses of fuel, cladding and fission product behavior, and temperature distribution.

2. To obtain the spatial distribution of heavy metal isotopes in the fuel rod at the end of the irradiation test. These calculated isotopic distributions will be compared with the measured values from the PIE.

Very high burnup (~ 110 MWd/kg) was achieved when the test was completed. Postirradiation examination is being carried out at ANL, and a portion of the preliminary results are given in Ref. 2-1.

The GB-10 capsule was irradiated in the poolside facility at the ORR. Figure 2-9 illustrates the approximate location of the capsule in the ORR, and Fig. 2-10 shows a cross section through the fuel portion. Table 2-5 gives the dimensions of the capsule at operating temperature. To account properly for out-leakage of neutrons into the water pool, a partial mock-up of the ORR core is required.

Figure 2-11 shows the geometric layout of the calculational model. The two-dimensional XY-geometry transport theory code DOT (Ref. 2-2) was used for the calculation. The transport model is based on discrete ordinates theory using S_4 quadratures and P_1 scattering expansion. Twenty energy groups (11 fast and 9 thermal) were used, and the P_1 cross sections in DOT were generated with the MICROX (Ref. 2-3), GGC-5 (Ref. 2-4), and GTF (Ref. 2-5) codes. To obtain nuclear cross sections for conditions near the end of the irradiation test, the MICROX code (Ref. 2-6) was used to perform a unit cell burnup calculation to obtain fuel rod atom densities at 110 MWd/kg burnup. These atom densities were then input into MICROX to obtain broad group nuclear cross sections. MICROX is a two-region unit cell spectrum code which solves 92 fine energy group fluxes in the fast range and 101 group fluxes in the thermal range. The fine group cross sections are then collapsed to broad group cross sections with the fine energy group fluxes. However, the output broad group cross sections are averaged over the cell, and therefore both the energy and geometric self-shielding effect are included in the cross sections. In the DOT calculation, the fuel rod is explicitly modeled. In effect, using the cell-averaged cross sections in the DOT model geometrically self-shields the fuel rod twice. To rectify

Fig. 2-9. Location of GB-10 capsule in ORR core

Fig. 2-10. Cross section through fueled portion of capsule GB-10

TABLE 2-5
 DIMENSIONS AT OPERATING TEMPERATURE FOR A CROSS SECTION
 THROUGH THE FUELED PORTION OF CAPSULE GB-10

	O.D. at Operating Temperature (mm)
Fuel	7.82
Cladding	9.07
Inner NaK	11.35
Zircaloy-2	18.85
Outer NaK	22.99
Inner 304 stainless steel	26.95
Helium	26.98
Outer 304 stainless steel	31.04

Fig. 2-11. Geometric layout of two-dimensional model

the situation, homogeneous fuel atom densities are used while keeping the $(U + Pu)/H$ ratio the same. Because of the crudeness of the MICROX model, 20 broad energy groups were used. Table 2-6 gives the broad energy group structure.

The broad group cross sections for the ORR core were obtained from Ref. 2-7. The GGC-5 (fast spectrum) and GTF (thermal spectrum) calculations were rerun for the broad group structure given in Table 2-6, and these cross sections were combined using the GIP code (Ref. 2-8) to give cross sections for DOT. Atom density distributions inside the fuel rod were obtained from PIE isotopic measurement of sample GB-10-175A17 (Ref. 2-1).

Figure 2-12 shows a plot of the plutonium fraction in the total heavy metal, and Fig. 2-13 shows the actinide isotope distribution in sample 175A17. These two figures were derived from two different measurement techniques (electron and ion microprobe). Comparison of the plutonium fraction in the two figures shows an inconsistency. This potential difficulty can be avoided when the data are used as follows. The plutonium content in Fig. 2-12 is divided among the plutonium isotopes according to the relative fractions given by the ion probe data in Fig. 2-13. Similarly, the uranium content derived from the data in Fig. 2-12 is split between U-235 and U-238 according to their relative fractions, given in Fig. 2-13. The ORR atom densities used in the model were calculated from the isotopic contents near the end of the irradiation period.

Figures 2-14 through 2-17 are plots of local power as a function of angle (θ) for four increments of radius (R). At $\theta = 0$ deg, the rod surface is farthest from the ORR core; it is nearest at $\theta = 180$ deg. Local power is normalized to a rod average power of 48 kW/m, which is the average linear heat rate of the GB-10 rod at the end of the irradiation period. Figure 2-18 is a plot of local power as a function of rod radius at $\theta = 180$ deg. At EOL, power increases inward from the rod surface, reaches a maximum at $R/R_o = 0.70$, and then decreases with decreasing radius. The initial

TABLE 2-6
BROAD ENERGY GROUP STRUCTURE

Fast Spectrum		Thermal Spectrum	
Group	Lower Energy (eV)	Group	Lower Energy (eV)
1	6.087×10^5	11	2.38
2	9.12×10^3	12	1.125
3	3.35×10^3	13	1.050
4	1.23×10^3	14	1.000
5	4.54×10^2	15	0.850
6	2.75×10^2	16	0.490
7	1.01×10^2	17	0.300
8	3.73×10^1	18	0.140
9	1.37×10^1	19	0.050
10	5.04	20	0.000

Fig. 2-12. Measured plutonium content, sample 175A17

Fig. 2-13. Isotopic fractional abundance, sample 175A17

Fig. 2-14. Angular dependence of linear heat rate; radial section = 0.352 to 0.390

Fig. 2-15. Angular dependence of linear heat rate;
radial section = 0.352 to 0.286 cm

Fig. 2-16. Angular dependence of linear heat rate;
radial section = 0.204 to 0.286 cm

Fig. 2-17. Angular dependence of linear heat rate;
radial section = 0.100 to 0.204 cm

Fig. 2-18. Linear heat rate as a function of radius ($\theta = 180$ deg)

increase in power is due to the increasing plutonium isotopes with decreasing radius (Figs. 2-12, 2-13). However, at the radius near the rod center, the effect of thermal flux self-shielding becomes dominant.

Figures 2-14 through 2-17 show the variation of local power when θ is small. A maximum variation of 10% [$\text{maximum}/(\text{minimum} - 1)$] in power can be seen at the rod surface. However, a maximum variation of 30% was calculated from GB-10 preirradiation analyses (Ref. 2-9). Figure 2-19 explains the difference. Figure 2-19 shows neutron fluxes as a function of distance from the ORR core for groups 12, 16, 18, 19, and 20. Groups 18, 19, and 20, the lowest thermal energy groups, account for the majority of neutron fissioning in GB-10 (due to large fission cross sections and fluxes); group 12 is the highest thermal energy group. The lower thermal energy neutrons (groups 19 and 20) have a peak 2.0 cm from the ORR core, and the high thermal energy fluxes decrease rapidly with distance from the ORR core. Near the end of the irradiation period, the center of the GB-10 rod was approximately 2.8 cm from the ORR core, which is quite close to the peak of the group 18, 19, and 20 fluxes, where the flux gradient is small. This explains the flatness of power across the rod.

The calculation performed in Ref. 2-9 simulates fuel rod conditions at the beginning of the irradiation period. At this time, the center of the GB-10 rod was located ~10 cm from the ORR core and was moved progressively closer to the ORR core to compensate for fuel burnup while maintaining fairly constant power. The thermal neutron flux gradient 10 cm from the ORR core is significantly greater than the gradient 2.8 cm from the core and should therefore result in a greater power gradient across the fuel rod.

An additional calculation was performed to duplicate the preirradiation calculation given in Ref. 2-9. In the additional calculation, the fuel rod was completely fresh and its center located 8.7 cm from the ORR core. Instead of the 30% gradient calculated in Ref. 2-9, a 25% gradient was calculated. The difference might be due to the different energy group structures used in the two models. Nine thermal energy groups (Table 2-6)

Fig. 2-19. Thermal neutron flux vs distance from aluminum wall

were used in the additional calculation; Ref. 2-9 used only three thermal energy groups having the energy group structure shown in Table 2-7. Neutrons belonging to energy group 4 of Ref. 2-9 have an energy between 0.1 and 2.34 eV, which includes groups 12 to 18 and part of 19. Using only one energy group for an energy between 0.1 and 2.34 eV failed to account for increasing softening of the thermal spectrum with increasing distance from the ORR core (Fig. 2-18). This results in overestimation of the power gradient across the fuel rod.

The one-dimensional transport code LOCALUX (Ref. 2-10) was used to perform a one-dimensional burnup analysis for the GB-10 fuel rod. Figures 2-20 through 2-24 are plots of the calculated and measured spatial distribution of heavy metal isotopes in the fuel rod at 110 MWd/kg burnup. In general, the calculations and measurements agreed reasonably well, although the following differences were noted:

1. The measured plutonium isotopes near the center of the rod were significantly higher than the calculated values, which may be due to migration of plutonium isotopes to the central void. This was not taken into account in the calculation.
2. The measured U-235 densities were slightly higher than the calculated values.

The calculated power within the fuel rod will serve as input to the analysis of fuel rod temperatures and, subsequently, fission product behavior.

2.1.1.5.2.2. Fission Products in GB-10 Traps. The relative isotopic distributions of several fission products in the GB-10 fuel rod determined from the γ -ray spectral analysis at the end of irradiation have been converted to an absolute basis (Table 2-8). The quantification was derived from the fission product inventory based on burnup measurements and axial γ -scan data. The calculated Cs-137 content (38.1 mg) in the charcoal trap is in good agreement with the mass spectrometric analysis of trap material

TABLE 2-7
ENERGY GROUP STRUCTURE USED IN REF. 2-9

Group	Lower Energy (eV)
1	4.98×10^5
2	9.12×10^3
3	2.34
4	0.10
5	0.05
6	0.0

Fig. 2-20. Comparison of U-235 densities

Fig. 2-21. Comparison of U-238 densities

Fig. 2-22. Comparison of Pu-239 densities

Fig. 2-23. Comparison of Pu-240 densities

Fig. 2-24. Comparison of Pu-241 densities

TABLE 2-8
AXIAL DISTRIBUTION OF FISSION PRODUCTS IN CAPSULE GB-10(a)

Isotope	Axial Distribution (mg)									Charcoal Trap
	Lower UO ₂ Pellet Region ^(b)					Fuel Region	Upper UO ₂ Pellet Region			
	Natural	5%	Natural	8.3%	14.9%		14.97%	8.37%	Natural	
I-131	0.53	1.42	0.712	1.42	2.49	153.0	6.94	3.20	5.16	1.78
Cs-134	7.26	7.26	6.05	35.9	15.7	197.7	10.1	15.9	52.5	47.2 (40.5) ^(c)
Cs-137	0.79	3.93	3.93	28.7	9.43	229.8	7.85	34.6	35.7	38.09 (37.2) ^(c)

(a) These data are based on gamma scans.

(b) Percentages are U-235 enrichment.

(c) The parenthetical values are from mass spectrometric analysis for Cs-133 and Cs-137.

(37.2 mg) given in Table 2-9. An estimate of the Cs-133 distribution was made using the Cs-134 γ -scan data as a reference monitor. The calculated Cs-133 content (47.12 mg) is approximately 15% greater than the mass spectrometric direct measurement of the trap material (40.77 mg). Table 2-9 gives the cesium and rubidium isotopic contents of the charcoal trap (isotope content is expressed as the percent of the isotope inventory produced by fission). Note the depressed Cs-135 content and the isotope percent found in the trap relative to the Cs-133 and Cs-137 values. The isotopic fission yields for these three isotopes are approximately equal. The smaller Cs-135 value is attributed to the ~48% neutron absorption burnout of the precursor Xe-135, which has a thermal neutron cross section of 7.6×10^6 barns.

The specific trap loading, 221.2 mg/g carbon (cesium + rubidium) is the highest yet observed and represents 8.1% of the cesium inventory and 3.2% of the rubidium inventory. This high-release fraction is probably due to sweep mode testing over the fuel region throughout the irradiation experiment. The fractional cesium inventory of the GB-10 of 0.081 is 72 times greater than the GB-9 value. Although the GB-9 capsule had only half the burnup of GB-10, it also had no sweep testing from the fuel region to the charcoal trap. These data need to be assessed in more detail and correlated with other experimental PIE measurements. Axial cesium profiles in the GB-10 axial blanket pellets are needed to complete the analysis of the cesium transport process.

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.

TABLE 2-9
CESIUM AND RUBIDIUM ISOTOPIC LOADING OF GB-10 CHARCOAL TRAP
BASED ON MASS SPECTROMETRIC ANALYSIS

	Cs-133	Cs-135(a)	Cs-137	Cesium Total	Rb-85	Rb-87	Rb Total
Loading (mg)	40.47	21.33	37.20	99.0	0.933	2.00	2.93
Specific Loading (mg/g carbon)	87.8	46.3	80.7	214.8	2.02	4.34	6.36
Inventory, calculated (mg)	403.6	420.7	392.7	1217.0	31.59	59.16	90.75
Percent found on trap	10.0	5.07	9.47	8.13	2.95	3.38	3.23

(a) Depletion of Cs-135 is due to burnout of the Xe-135 precursor.

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, and the first phase of the irradiation was completed in midrun 100. An estimated burnup of ~4.7 at. % and a fluence of 5×10^{26} n/m² was accumulated during this phase of irradiation. During the first interim examination the assembly was radiographed and profilometry measurements made. The assembly was dismantled and the rods were visually examined.

During this quarter, fuel rod profilometry was completed, and the following observations were made:

1. The maximum increase in diameter was ~0.05 mm.
2. The top one-third of the fueled section (the high cladding temperature regime) did not show any significant diametral growth.
3. Cladding swelling appeared to be initiated at this fluence. This is not unusual, since the cladding may have an incubation fluence of 5×10^{26} n/m².
4. The maximum diametral change occurred in fuel rod GH-77.

5. There was no evidence of unusual bulges near the fuel-blanket interface.
6. Profilometry data were reduced to average values for five equally divided sections of the fuel rod for use in LIFE code analysis (Ref. 2-11).

Residual bow measurements on each of the 31 rods have been completed. A maximum bow of ~24 mm was observed for rods GA-43 and GA-44, which are the rods farthest away from the core center.

Rods GH-77 and GD-60, designated for destructive examination during the first interim examination, have been shipped to ANL. At ANL, the rods were visually examined, and a detailed gamma scan for long-lived fission products was performed. Earlier gamma scan data from the hot fuel examination facility (HFEF) are being processed at GA. The 19-rod subassembly for the low-power irradiation phase of the experiment has been fabricated and shipped from GA to EBR-II with dummy fuel rods for initial flow tests.

Agreement has been reached between ANL and GA on the 14 fuel rods to be depressurized during the first depressurization, and the rods were shipped to Hanford Engineering Development Laboratory (HEDL) for depressurization. The equipment (air drill setup) for drilling the fuel rod plug was designed, assembled, and tested prior to shipment to HEDL, and a small lathe was modified for machining the fuel rod end plug. The lathe setup was tested in the laboratory using a dummy fuel rod prior to shipment to HEDL. All equipment has been installed in the hot cell at HEDL.

HEDL has performed trial welds on irradiated and unirradiated stock to define weld parameters and weld qualification. The unirradiated welds were successful, but there was a cracking problem with the irradiated welds. This problem is being investigated. If the trial welds are satisfactory, the fuel rod depressurization and resealing operation will be initiated.

2.1.1.5.4. HELM Bundle Experiments

2.1.1.5.4.1. HELM 2 Fission Gas Release and Venting Analysis. This task was completed during the last quarter (Ref. 2-12).

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

There was no activity on this task during this quarter.

2.1.2.1.2. Assembly Mechanical Tests

There was no activity on this task 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 date have generally been favorable, specific tests are required to assess actual flow-induced vibration levels.

The helium flow test rig (HFTR) is a high-pressure helium system capable of testing up to 37-rod fuel bundles at full flow. The rig can also be used to test smaller bundles. Figure 2-25 is a schematic of the rig. The operating parameters of the rig are as follows:

Helium flow rate	0 to 0.8 kg/s
Helium pressure	0 to 6 MPa
Helium temperature	20° to 200°C

Fig. 2-25. Helium flow/vibration test rig

Single-rod and 19-rod bundle tests in the HFTR are planned. The design of the test rig and the test bundle for the first sequence of tests was completed and fabrication initiated, and the design of the test bundle for the second sequence of tests was begun. The test specification for the single-rod tests was also completed.

The HFTR will be located in the helium circulator test facility at GA. The rig consists of a high-pressure helium circulator, a water-cooled helium heat exchanger, a test vessel, and associated piping and valving. Figures 2-26 through 2-28 show the layout of the rig. All major components, including the circulator, cooler, piping, and major valves and flanges have been received. The water supply system and the electrical supply system have been installed, and the circulator and motor have been mounted on their base pad. The scaffolding for accepting the test vessel has been constructed and is ready for installation. Figure 2-29 shows the test vessel. It consists of an 8-in. schedule 450 pipe which is about 3 m long. Large flanges are welded to each end of the pipe for mounting and access, and small penetrations are provided for routing of instrument leads. Figure 2-30 shows the single-rod vibration test assembly mounted in the test vessel. It consists of a flow tube which guides the flow around the instrumented simulator rod and also houses the spacer grids, which provide the lateral support for the rod. The flow tube is mounted to a U-shaped strong back with U-bolts to prevent damage to the test section during handling. Eddy current proximity probes are attached to the flow tube at various elevations. These probes are attached to the flow tube at various elevations and accurately determine the position of the rod within the flow tube (these data will be correlated with displacement information obtained from accelerometers located within the rod simulator). The rod simulator (Fig. 2-31) consists of ribbed stainless steel tubing which has an 8-mm o.d. (over the ribbing), is ~3 m long, and is filled with lead-antimony pellets to simulate the mass of GCFR mixed oxide fuel. At selected locations, a few mass simulator pellets are removed and replaced with an instrument pellet holding a pair of miniature uniaxial accelerometers. Each rod will house up to four pairs of accelerometers.

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Fig. 2-27. Helium flow test rig

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Fig. 2-28. Helium flow test rig

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NOTES
 TO BE DETERMINED BY TEST PROCEDURE
 DOCUMENT NO. (LATER)

1	26	WIRE .063 X .111	SS
2	25	WASHER .4810	
3	24	NUT HEX 5/8-16	
4	23	BOLT HEX HD 1/2-13X1.5	
5	22	BOLT HEX HD 5/8-11X1.5	
6	21	BOLT HEX HD 3/4-10X2.0	
7	20	NUT HEX 5/8-10X1.5	
8	19	NUT HEX 3/4-20	SS
9	18		
10	17	CONDUIT ASSEMBLY	
11	16	1/2" BOLT	
12	15	1/2" BOLT	
13	14	1/2" BOLT	
14	13	1/2" BOLT	
15	12	1/2" BOLT	
16	11	1/2" BOLT	
17	10	1/2" BOLT	
18	9	1/2" BOLT	
19	8	1/2" BOLT	
20	7	1/2" BOLT	
21	6	1/2" BOLT	
22	5	1/2" BOLT	
23	4	1/2" BOLT	
24	3	1/2" BOLT	
25	2	1/2" BOLT	
26	1	1/2" BOLT	

ITEM	PART NO	DESCRIPTION	MATERIAL SPEC
1		SECT A-A	
2		SECT B-B	
3		SECT C-C	
4		SECT D-D	
5		SECT E-E	
6		SECT F-F	
7		SECT G-G	
8		SECT H-H	
9		SECT I-I	
10		SECT J-J	
11		SECT K-K	
12		SECT L-L	
13		SECT M-M	
14		SECT N-N	
15		SECT O-O	
16		SECT P-P	
17		SECT Q-Q	
18		SECT R-R	
19		SECT S-S	
20		SECT T-T	
21		SECT U-U	
22		SECT V-V	
23		SECT W-W	
24		SECT X-X	
25		SECT Y-Y	
26		SECT Z-Z	

Fig. 2-30. Single-rod vibration test assembly

Fig. 2-31. Fuel rod assembly simulator

Because of the possible influence of clearances and gaps between components on system damping and, hence, rod flow-induced vibration amplitudes, special measures are being taken to ensure that either the clearances are eliminated or, where that is not feasible, the dimensions and tolerances are controlled to provide a given clearance with a minimum of variation. Examples of such cases include (1) the spacer grids, which will be positioned in the flow tube by set screws to eliminate slack (the flow tube itself is honed to achieve an accurate i.d.); (2) the pellets, which will be machined for minimum clearance after the i.d. of the cladding is measured; and (3) the grids, which will be sized after the o.d. of the cladding is measured.

The tests planned for the HFTR include single-, 19-, and 37-rod flow and vibration tests. The general objective of these tests is to measure the vibration amplitudes of single simulated fuel rods in a flowing helium environment which simulates a GCFR operating environment. The effect of assembly design changes on rod response will be also evaluated. The specific objectives of the single-rod tests are as follows:

1. To measure the dependence of rod vibration amplitude on helium flow rate in the range 50% to 150%.
2. To determine the dependence of rod vibration amplitude on the span between the grids. Spans of 200, 300, and 400 mm will be considered.
3. To determine the dependence of rod vibration amplitude on the clearance allowed between the fuel rods and the spacer grids. Diametral clearances of 0.1, 0.2, and 0.3 mm will be considered.
4. To measure the difference in rod vibration amplitude between fuel rod simulators having smooth cladding and those with cladding ribbed with GCFR-type heat transfer ribs over the full length of the rod.

The vibration measurements will be obtained by internal rod accelerometers and external eddy current proximity probes. Helium flow rate, temperature, and pressure will also be measured. The pressure measurements will include static and fluctuating components. The time history of the data will be displayed, and the data will be reduced using a two-channel real time fast Fourier transform analyzer to obtain power spectral density profiles and cross correlation (or coherence) plots.

2.1.2.1.4. Component Flow Tests

There was no activity on this task during this quarter.

2.1.2.1.5. Core Flow Test Loop

A series of out-of-pile simulation tests will be conducted to (1) demonstrate the ability of the GCFR fuel, control, and blanket assembly designs to meet design goals and (2) verify predictions of analytical models which describe 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 progressively be extended to the highest possible temperature until the heater elements fail. The core flow test loop (CFTL) program plan (Ref. 2-13) describes the requirements for the test program to be conducted in the CFTL, which will be constructed and operated by ORNL.

Efforts are being made to start testing in the fall of 1981, which is the target date for ORNL to complete the stage I configuration of the CFTL. The stage I CFTL will be capable of performing all 37-rod bundle, AG-1 tests to simulate normal, upset, emergency, and faulted GCFR conditions except rapid depressurization.

Work on thermal-flow-structural analysis of the test models and the loop has been in support of the AG-1 design. The TSPEC code (Ref. 23-14) is being brought into agreement with current GCFR and ORNL methods.

2.1.2.1.5.1. CFTL Mechanical Analysis. A thermal bowing analysis was performed for a typical edge rod of the revised CFTL bundle AG-1. The analysis employed the CRASIB code (Ref. 2-15) and considered the rod to be a variable cross section beam. The spacer grids were modeled as ideal simple supports. Figure 2-32 shows the spacer grid arrangement, which consists primarily of a uniform 200-mm pitch between grids. The mechanical data for the simulator rod analyzed are as follows:

Rod o.d. (in smooth regions)	8.0 mm
Rod root o.d. (in ribbed regions)	7.74 mm
Rod i.d.	6.98 mm
Rod length	2860 mm
Heater length	1200 mm

Cladding temperatures and temperature gradients for this study were obtained from a COBRA (Ref. 2-16) analysis of the revised AG-1 bundle design operating under normal, steady-state, and uniform power conditions, as described in Section 2.1.2.1.5.2 and shown in Fig. 2-33. The analysis indicates that the revised AG-1 bundle design will result in acceptable levels of rod bowing for the operating condition considered. Figure 2-34 shows the final bowed shape of the rod, and a summary of other results is given below. (All the load and deflection results are satisfactory.)

Maximum rod bow	0.11 mm
Maximum rod slope	0.09 deg
Maximum bending stress	36.0 MPa
Maximum rod-spacer interaction force	2.6 N

2.1.2.1.5.2. AG-1 Thermal Analysis. As a follow-up to the work described in Ref. 2-12, the revised AG-1 bundle design was analyzed with COBRA using the one-sixth bundle section which had previously produced the greatest rod diametral temperature difference. The revised AG-1 design differs from the previous design in that it has a 1200-mm heated length (previously 1400 mm) and new spacer locations. Otherwise, the major features are identical, i.e., rod diameter = 8.0 mm; rod pitch = 12.0 mm;

Fig. 2-32. CFTL bundle AG-1 spacer grid arrangement

Fig. 2-33. CFTL bundle AG-1 edge rod cladding temperatures and temperature gradients

Fig. 2-34. CFTL bundle AG-1 edge rod thermal bowing

edge spacing = 80%, roughening of the heated length = 100%; a true hex spacer geometry; and three internal support rods. For the revised AG-1 design, the maximum rod diametral temperature difference was 41°C for the corner rod and included the effects of grid spacer heat transfer enhancement and rod circumferential conduction.

Temperature-length plots for three edge rods and two internal rods aligned with the corner rod were used for rod stress and bowing analyses. Stresses and rod bowing were calculated to determine the thermal bowing of heater rods under CFTL conditions. The same rod temperature distribution was used to calculate rod bowing in a GCFR-type nuclear environment by imposing the appropriate irradiation flux, metal swelling, and creep. Section 2.3 reports the results of this calculation.

To determine the effect of design basis depressurization accident (DBDA) conditions on the revised AG-1 bundle design, the pressurized DBDA (3.75% flow, 2% power) and the depressurized DBDA (2% flow, 4% power) under quasi-steady-state conditions were analyzed using the latest COBRA code version, which includes internal calculation of both circumferential conduction and spacer heat transfer enhancement at each axial location. Because of the low rod surface heat transfer coefficients during laminar flow and the high thermal conductivity of the boron nitride within the heater rod, the maximum rod diametral temperature differences are only about 5°C if duct heat loss is neglected. Duct heat losses for the two conditions were calculated to be about 5% of the heat input for the pressurized case and about 10% for the depressurized case. This information will be incorporated into additional COBRA runs to determine the effect of heat loss on the AG-1 rod diametral temperature difference during DBDA.

2.1.2.1.5.3. AG-1 Design and Fabrication. The AG-1 design was updated; Figs. 2-35 through 2-38 show the test section assembly, skeleton assembly, spacer grid, and rod simulator cladding. Fabrication of the test section parts for AG-1 [hanger (support) rods, spacer tubes, hanger rod end caps, nuts for the skeleton assembly, load pads for duct lateral restraint, and cladding for rod simulator] continued. The bundle duct and spacer grids will be fabricated this year.

GA DRAWING B2478-03

CONVERSION TABLE

MILLIMETER	INCH
3	.12
5	.20
200	7.87
250	9.84
300	11.81
400	15.75
720	28.35
940	37.01
1200	47.24
1220	48.03
2050	112.00
3000	157.00

- 001

FUEL ROD SIMULATOR POSITIONING

ALL MEASUREMENTS IN MM

12	A	024 700	CHILD PROTECTIVE PLACING	
1	Z	024 700-001	CHILD PROTECTIVE PLACING	
1	A	024 700	CHILD PROTECTIVE PLACING	
	ITEM	PART NO	DESCRIPTION	MATL/MATL SPEC
	PART ITEM ATTACHED DOCUMENT			
		024 700		
REODI/ASSEMBLY	LOG	SYSTEM	UNIT	PRIORITY
				PARTS LIST

Fig. 2-35. 37-rod fuel test section

[illegible]

ALL MEASUREMENTS IN MM

- ▶ DRIVEN ENDS OF SPOKE GIRD: PRIOR TO ASSEMBLY TO BE (0.20 MM) ALMOST FLAT LFT: THUS UNFRAISED HEE DIRT ASSY. ID. ENG. 111-024029
- ▶ PRIOR TO ASSEMBLY, SPACER TUBES SHALL BE MACHINED IN SETS OF (3) WITHIN 0.01MM OF EACH OTHER. MARK EACH SET OF (3) TUBES WITH ADEQUATE IDENTIFICATION. ASSEMBLE IN SETS TO KEEP MISALIGNMENT OF SPACERS WITH RESPECT TO SURFACE PLATE DATUM -A- TO A MINIMUM. (REF DWG. NO'S. 024800 & 024801).
- ▶ TORQUE NUTS TO 2.4 N-m.
- ▶ THE 40.4 MAX. DIM SHOWN REPRESENTS THE MINIMUM SPACER GRID THICKNESS & MINIMUM SPACER TUBE LENGTH TOLERANCE STACK-UP CONDITIONS.

ITEM	PART NO.	DESCRIPTION	MAT/MATL SPEC
1	0210000001	SHUT	
2	0210000003	DISCONNECT, SHUT OFF TIME	
3	0210000001	DISCONNECT, SHUT OFF TIME	
4	0210000002	DISCONNECT, SHUT OFF TIME	
5	0210000001	SHUT OFF TIME	
6	0210000004	SHUT OFF TIME	
7	0210000006	SHUT OFF TIME	
8	0210000002	SHUT OFF TIME	
9	0210000005	SHUT OFF TIME	
10	0210000001	SHUT OFF TIME	
11	0210000001	SHUT OFF TIME	
12	0210000001	SHUT OFF TIME	
13	0210000001	SHUT OFF TIME	
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86	0210000001	SHUT OFF TIME	
87	0210000001	SHUT OFF TIME	
88	0210000001	SHUT OFF TIME	

Fig. 2-36. CFTL bundle AG-1 skeleton assembly

NOTES

1. **-B-** IS THE CENTER OF THE TRIANGLE FORMED BY THE CENTERS OF HOLES #1, 2 & 3.
2. **-D-** OCCURS (34) PLACES
3. **-E-** OCCURS (3) PLACES
4. **-F-** & **-D-F** OCCUR (102) PLACES
5. THE 76.85 MAX DIA. OVER THE (0.05) THK OUTER FRDS MUST BE FINAL MACHINED ACROSS FLATS TO BE 0.20 ± 0.05 LESS THAN MEASURED MAX DIRT ASSY I.D. AVERAGE. (DIMS NO. 025 025) PAD THICKNESS TO BE EQUAL TO EACH OTHER WITHIN 0.05.

ON DRAWING 024790	11.50	.4528
CONVERSION TABLE	12.00	.4724
	15.0	.591
MILLIMETER INCH	18.00	.7087
	.02	.0008
	.04	.0016
	.05	.0020
	.1	.004
	.18	.0070
	.25	.0098
	.3	.012
	.4	.0157
	.5	.0177
	.625	.0248
	.75	.0295
	.875	.0343
	1.0	.0394
	1.25	.0492
	1.5	.0591
	1.75	.0689
	2.0	.0787
	2.5	.0984
	3.0	.1181
	3.5	.1378
	4.0	.1575
	4.5	.1772
	5.0	.1969
	5.5	.2166
	6.0	.2363
	6.5	.2560
	7.0	.2757
	7.5	.2954
	8.0	.3151
	8.5	.3348
	9.0	.3545
	9.5	.3742
	10.0	.3939

ITEM	PART NO	DESCRIPTION	MATL/MATL SPEC
REQUISITION	024795	SPACER GRID	531600
DISC SYSTEM	IN	DISC	DISC
DISC SYSTEM	IN	DISC	DISC

Fig. 2-37. CFTL bundle AG-1 spacer grid

NOTES:

- 1 - ORNL SHALL CUT AND MODIFY BOTH ENDS OF FUEL ROD SIMULATOR CLADDING AT ASSY.
- 2 - TRANSITION REGION OF IMPERFECT RIB FORM.
- 3 - DIRECTION OF HELICAL ROUGHENING IS OPTIONAL
- 4 - 316 SS, 20% ± 5% COLD WORKED PER GA. SPEC 903944 - C

GA. DIMENSIONS 82-7288 3085 138.07

CONVERSION TABLE

MILLIMETER INCH

.01 .0004
 .02 .0008
 .03 .0012
 .05 .0020
 .06 .0024
 .10 .0039
 .12 .0047
 .13 .0051
 .20 .0079
 .25 .0098

.45 .0177
 .48 .0189
 .5 .0200
 1.0 .0394
 1.5 .0591
 2 .0787
 5 .1969
 6 .2362
 8 .3150
 7.74 .3047
 8.00 .3150
 12 .4724
 25 .9843
 765 30.12
 1220 48.03

ALL MEASUREMENTS IN MM

					FUEL ROD SIMULATOR CLADDING		
					ITEM	PART NO.	DESCRIPTION
					NEXT INDENTURED DOCUMENT		
					024794		
					PARTS LIST		
					REQD/ASSEMBLY	DISC	SYSTEM
						IB	TYPE
						DGE	PROJECT
							6113

Fig. 2-38. CFTL bundle AG-1 fuel rod simulator cladding

2.1.2.1.6. Assembly Prototype Tests

The core assembly prototype tests are defined in Refs. 2-17 and 2-18. The tests will be conducted on full-size core assemblies to ensure that they meet design qualification requirements prior to fabrication of the demonstration plant initial core. The prototype assemblies will be the same as the GCFR demonstration plant core assemblies except that the PuO_2 - UO_2 fuel in the GCFR fuel rods will be simulated by depleted UO_2 . The assemblies will be subjected to maximum GCFR helium flow conditions to simulate the reactor core environment closely; however, there will be no radiation. One assembly of each type (fuel, control, and blanket) will be subjected to the equivalent of approximately one year each of reactor operation in the hot helium test loop. The helium test loop temperature will be maintained external to the test section, since fuel rod heating will not be simulated in these tests.

2.1.2.1.7. Depressurized Accident Condition Test (DACT)

The DACT will provide design basis accident data and core cooling safety margin information under low-pressure, pseudo-steady-state, depressurized conditions. Experimental information on potential GCFR core assembly edge channel problems and flow and temperature distributions within the bundles under depressurized conditions will also be provided. In addition, DACT will be capable of investigating natural convection effects within the core assemblies. The DACT will be accomplished before the CFTL DBDA tests and will provide the near-information required for licensing reviews. Since recently developed experimental equipment for the steel melting and relocation tests (SMART) can be adapted to DACT, it is proposed that DACT be conducted at Los Alamos Scientific Laboratory (LASL), which is including the DACT requirements in the design of the SMART test vessel.

Successful completion of the 37-rod bundle SMART experiment (FLS-2) will ensure that the SMART equipment design and fabrication techniques are suitable for the DACT experiment. A set of test operating conditions for

FLS-3 has been suggested. It will provide input for a COBRA code prediction check and the information necessary for verifying the adequacy of the thermocouples at temperatures approaching that of cladding melting.

2.1.2.2. Pressure Equalization System.

2.1.2.2.1. PES Seals

There was no activity on this task during this quarter.

2.1.2.2.2. Fission Product Plateout and Plugging

There was no activity on this task during this quarter.

2.1.2.2.3. Fission Product Release and Transport

There was no activity on this task during this quarter.

2.1.2.2.4. Monitor Station and Instrumentation

Research and development is being conducted to establish design criteria and to acquire design information for the monitor stations of a proposed GCFR demonstration plant and subsequent commercial reactors. The stations monitor the radioactivity of gaseous fission products passing through the lines of the pressure equalization system (PES) after coming from the vent holes of the core assemblies. They also monitor a matrix-connected piping system to the core subassemblies and indicate the location and leak rates of leaking subassemblies by using the concentrations of radioactive gaseous isotopes in the sweep gas. A monitor station is being assembled for acquisition of radioactivity (photon emission) data from fission product gas mixtures being vented from the GCFR 12-rod fuel bundles being irradiated in the helium loop in the BR 2 reactor at Mol, Belgium (HELM).

The BR 2 reactor is currently shut down for replacement of the beryllium moderator. Design and assembly of the monitor station is proceeding prior to continuing irradiation of the HELM 3 bundle. Although the initial application of the monitor station is to HELM, it will subsequently be used, perhaps with modifications, to monitor other irradiation tests. The monitor station is being designed and built by GA for installation on HELM. [HELM is installed in the BR 2 test reactor at the Centrum d'Etudes Nucleares (CEN).] HELM was built to test GCFR pressure-equalized and vented fuel bundles at prototypical conditions. However, a cadmium-filtered neutron spectrum replaces the fast neutron spectrum.

The monitor station detector will be mounted on the sweep gas line carrying gaseous fission products vented from the fuel bundle to the HPS (Fig. 2-39). This arrangement is analogous to that of the GCFR demonstration plant monitor stations which will monitor the activity vented from the GCFR core and swept through the monitor lines to the HPS.

2.1.2.2.4.1. Purpose and Function. Development of the monitor station will enable (1) development of design criteria for GCFR monitor stations, (2) acquisition of data for verifying the COUNT computer program (Ref. 2-19), which is being developed for GCFR monitor station design and has been used in the design of the GB-10 and the HELM monitor stations; (3) acquisition of design basis data for GCFR monitor stations; (4) gaining of experience with typical equipment and techniques which could be used in monitor station design; and (5) accomplishment of the foregoing under test conditions which give a good simulation of GCFR monitor station operating conditions, such as those provided by the PES of the HELM facility during testing of GCFR fuel bundles.

The function of the station is to monitor the radioactivity of the fission product gases being vented from the GCFR test fuel bundles and swept past the monitor station during all normal, steady-state, and transient operating conditions. The monitor station functions as a process instrument system. The station will initially monitor the HELM 3 bundle to obtain baseline (no leak) data over a burnup range of 28 to 65 MWd/kg (1.5

Fig. 2-39. Monitor station detector

yr), since HELM 3 is not expected to leak. Following HELM 3, it is planned to monitor the HELM 2A bundle, which will contain an artificially fabricated defect in the cladding of one rod with a known leak rate. HELM 2MO will be tested for 3 to 6 months (8 to 16 MWd/kg), and the station will measure the gamma photon spectra of the fission product mixtures in the sweep gas and indicate the rate of leakage in the fuel rod cladding. A significant amount of data will be collected at HELM and analyzed at GA. Data processing, control functions, alarm setting, and function and station tending duties will be performed by a microprocessor incorporated as a major component in the monitor station.

The monitor station is composed of four interconnected assemblies: (1) radiation detector and preamplifier; (2) multichannel analyzer; (3) microprocessor (μP); and (4) teleprinter with cassette deck magnetic tape for data storage. Figure 2-40 shows the arrangement of these assemblies.

2.1.2.2.4.2. Relationship of Monitor Station to BR 2 Reactor and HELM. The power level in the HELM bundles depends on the local power at the location of the bundle in the BR 2 core. The number of leaking rods and the individual leak rates can hopefully be controlled, but they must be designed and built into each bundle. They are not changeable in a single bundle without major rework in the hot cells. HELM 3 is not designed for any leaks, and to our knowledge, none currently exist. Thus, monitoring of HELM 3 represents a base case.

The monitor station for HELM 2A was initially designed for a defect of 2×10^{-2} g/s, or 1 milliliter/s, of helium coolant inleakage. If the design leakage is realized, it will represent nearly the maximum case of activity vented from a single rod leak. However, there is considerable uncertainty about the leak rate that can be achieved with a rod with an artificial defect.

Operation of HELM 2A with a leaking rod is a first attempt, and thus there are a number of significant unknowns. For example, fuel movement within the defective rod could block or restrict flow through the defect

Fig. 2-40. Monitor station

hole. Leakage flow cannot be separately and directly measured. Only the total flow rate in the sweep gas line can be measured. The total flow rate comprises the suction hole leakage plus leak flow rates. Because the HELM tests start with either no leaks or an active leak, simulation and monitoring of the transients anticipated in a GCFR when a leak starts will not be possible in HELM.

Three factors are involved in locating a leaking fuel subassembly and measuring its leak rate in a GCFR. First, only leaks which transport gaseous fission products faster by convection than by diffusion in non-leaking rods can be detected. For such leaks, gaseous fission products which previously filled the gas-phase volume of the fuel rods by diffusion prior to a leak will be swept out of the fuel subassembly, creating a pulse of activity in the sweep gas. The height and duration of the pulse will depend on the initial leak rate. The activity pulse detected by the monitor station will signal the advent of a leak. However, this phenomenon will not occur in the HELM monitor station test arrangement, since the leak in HELM 2A will be present at the start.

The second factor involved in locating a leaking fuel subassembly in the GCFR depends upon the connection of the two vents of each subassembly to different monitor lines in a matrix of monitor line connections. High activity levels in two monitor lines are necessary for locating a subassembly in the matrix of the core. Again, this does not occur in the HELM tests because only a single fuel bundle is tested at any time, and each bundle has only a single vent.

The third factor, and the one involved in the HELM monitor station, is that the steady-state activity vented to the sweep gas (after the gaseous fission product inventory in the rod prior to the leak has been dissipated) is very sensitive to the leak rate and provides a means for continually measuring leaks which are much smaller than could be measured any other way. Theoretically, the minimum detectable leak rate (the minimum leak rate is one which vents twice the activity vented under no-leak conditions) for the HELM fuel bundles is given by Xe-131m at $\sim 3 \times 10^{-5}$ milliliters/s,

and should be detectable from the Xe-131m signal. However, Xe-131m appears to be difficult to measure in the gamma photon spectrum. Xe-133 offers a minimum leak rate signal that can be measured at $\sim 7 \times 10^{-5}$ milliliters/s, and Xe-135 is certain to establish a measurable lower limit for a leak rate of $\sim 2 \times 10^{-4}$ milliliters/s for the HELM bundles. It is necessary to use a gamma photon spectrometer that can resolve the photopeaks of the spectra produced by activity flowing through the monitor or sweep gas line and past the gamma photon detector.

2.1.2.2.4.3. Leak Rate Measurement and Indication. Leak rates will be derived from spectral measurements, and photopeaks have been selected for their freedom from the interference of other photopeaks in the spectra and their leak rate correlations. The leak rate in the low flow rate range is determined by the absence or presence of selected photopeaks above threshold count rates. This is a consequence of the steep curves of count rate versus flow rate which are characteristic of the short-lived isotopes. At high leak rates, counting rates measured at the 456-keV photopeak of Xe-137 are correlated with the leak rates, since all other activities, except those for Kr-89, exhibit flat responses versus leak rate. The 456-KeV photopeak of Xe-137 is stronger and less subject to interference from other photopeaks and the underlying continuum in the gamma ray spectrum than the 221-keV photopeak of Kr-89. Threshold counting rates under no-leak conditions are established for each photopeak energy, and set points above the threshold are registered in the microprocessor (μP). When the counting rate reaches any of the set points, the microprocessor determines the maximum order-of-magnitude leak rate to be registered in the output.

At high flow rates, a different method of finding the order-of-magnitude leak rate must be used. The leak rate is correlated with the counting rate levels of a single photopeak energy, and the selected energy is the 456-keV photopeak of Xe-137. Count rate set points are established for each order-of-magnitude leak rate level covered; three set points would be required based on calculations. Whenever the count rate reaches a set point, the microprocessor registers the corresponding order of magnitude of

the leak rate in the output. The photopeak energies of interest and their calculated counting rates are correlated with the leak rates in Table 2-10. Only the order of magnitude of the leak rate is indicated. For example, a typical output would read $10^{-3} < Q_L < 10^{-2}$, where Q_L is the volumetric leak rate (milliliters/s).

Indication of a leak and leak rate estimation cannot be carried out when transient operations are in progress because of normal fluctuations under the transient conditions. Allowable operating bands are defined for variables that indicate transient conditions, i.e., bundle power, coolant gas outlet temperature, and sweep gas pressure. Whenever these variables move outside the allowable band, the microprocessor blocks indication of the occurrence of a leak and leak rate. Instead, the microprocessor indicates "transient in progress" in the output. However, all the operating data are recorded in the output in the normal manner. The widths of the bands which allow for normal steady-state fluctuations are based on HELM 3 experience for the three transient parameters: coolant outlet temperature $\pm 5^\circ\text{C}$, fuel bundle power $\pm 5 \text{ kW(t)}$, and sweep gas pressure $\pm 0.1 \text{ bar}$. These band widths have been accepted for design and initial use but may require adjustment depending on monitor station experience.

2.1.2.2.4.4. Leak Rate of Artificial Defect. A leak rate of $1 \times 10^{-2} \text{ g/s}$ ($\sim 1 \text{ milliliter/s}$) in the HELM 2A test was initially selected for a number of reasons: (1) it could be measured; (2) it produced an acceptable spectral complexity at the monitor station; (3) it would accelerate fuel bundle chemical reactions with coolant impurities; and (4) it would provide adequate testing of inleakage and fission product containment in the PES. The sharp-edged orifice equation used to estimate the size of an artificial cladding defect (hole) for the HELM 2A fuel bundle is taken from Ref. 2-20:

$$Q = C_v C_c A \sqrt{2gh} \quad ,$$

where Q = volumetric flow rate (m^3/s),

C_v = coefficient of velocity,

C_c = coefficient of contraction,

TABLE 2-10
LEAK SENSITIVITY OF HELM MONITOR STATION(a)

Isotope	Half-Life(b)	Photopeak Energy (keV)	Assumed Leak Rate [g/s (millimeters/s)]	Count Rate (Hz)
Xe-133	2.3d	81	0	86
Xe-135	9.2h	250	0	15
Xe-133	2.3d	81	10^{-6} (10^{-4})	216
Xe-135	9.2h	250	10^{-6} (10^{-4})	21
Xe-135	9.2h	250	10^{-5} (10^{-3})	1127
Xe-138	14.2m	258	10^{-5} (10^{-3})	26
Xe-138	14.2m	258	10^{-4} (10^{-2})	1997
Xe-137	3.9m	456	10^{-4} (10^{-2})	38
Xe-137	3.9m	456	10^{-3} (10^{-1})	1152
Xe-137	3.9m	456	10^{-2} (1)	2243

(a) This selection assumes the use of a coaxial germanium detector.

(b) m = minutes; h = hours, d = days.

A = orifice area (m^2),

g = gravitational acceleration (9.81 m/s^2),

h = head loss across orifice (m).

$$C_v = \frac{V_2}{\sqrt{2gh}},$$

where V_2 is the ideal velocity in the orifice.

For sharp-edged orifices with $0.95 < C_v < 0.99$,

$$C_c = \frac{A_{\text{vena contracta}}}{A},$$

where $A_{\text{vena contracta}}$ is the minimum flow just beyond the orifice.

For sharp-edged orifices with $0.61 < C_c < 0.72$,

$$C = \text{discharge coefficient} = C_v \cdot C_c.$$

C must be experimentally determined. For estimating the HELM 2M0 leak hole size, C was assumed to be 0.7, and the hole diameter for a 1×10^{-2} g/s leak was calculated to be ~ 0.25 mm, or 4 holes, each with a diameter of 0.12 mm. A number of small holes might be used to simulate a crack in the cladding.

Holes have been fabricated in fuel cladding to determine the discharge coefficients and the size of defects in fuel rods which will produce a leak rate of $\sim 1 \times 10^{-2}$ g/s (the leak rate desired for testing the monitor station). Holes measuring 0.18, 0.22, 0.25, 0.40, and 0.57 mm were fabricated by electrical discharge machining. The characteristic ΔP versus flow rate curve for each size hole will be determined so that the HELM 2M0 defect size can be selected.

2.1.2.3. Assembly Methods Development.

2.1.2.3.1. PES Methods Development

Development of the COUNT code continued with the programming of Bremsstrahlung ("braking radiation") and Compton contributions to the calculation of detector response to activity vented from the HELM 3 fuel irradiation test. COUNT is used to calculate the response of a monitor station detector in the PES of an in-pile irradiation test or GCFR (Ref. 2-12). The program accounts for release of fission gases from the fuel into the gas phase, venting of the gases from the fuel assembly, and dilution of activity by sweep gas and fuel rod leakage. The spectral response of a selected detector is then determined as a function of suction hole flow and leak flow rates. The response of a coaxial, intrinsic germanium gamma detector to be used with the HELM 3 test (Section 2.1.2.2.4) is being modeled with COUNT.

Bremsstrahlung is emitted as a continuum of x-rays which are produced when high-energy beta particles dissipate their energy while passing through matter. It contributes to the background count at the monitor station when beta particles produced from the decay of fission gases during transport through the monitor station strike the monitor line wall. The quantity of beta particle energy converted to Bremsstrahlung is approximated from the expression (Ref. 2-21)

$$E_B = (1.1 \times 10^{-3})(Z)(E_\beta)^2 \quad , \quad (2-1)$$

where E_B = Bremsstrahlung energy per disintegration (MeV),

Z = atomic number of monitor line tubing (assumed to be

Fe, $Z = 26$),

E_β = beta particle energy (MeV).

Note that the fraction of Bremsstrahlung increases with the square of the beta particle energy. The fraction of Bremsstrahlung at a given photon energy is calculated from the equation (Ref. 2-22)

$$R = \exp [(-5.71)(X) + 1.74] \quad , \quad (2-2)$$

where R = normalized fraction of Bremsstrahlung at X_λ ,

$$X = E_p/E_\beta \quad (X_\lambda < 0.8),$$

E_p = photon energy of interest (MeV),

E_β = beta particle energy (MeV).

The absolute number of Bremsstrahlung at a given photon energy is derived using the expression

$$P = \frac{(R)(E_\beta)(F_\beta)(\Delta E)}{E_p} \quad , \quad (2-3)$$

where P = number of Bremsstrahlung at energy E_p ,

F_β = fraction of beta decay of given isotope at E_β ,

ΔE = resolving power of detector (assumed equal to 0.001 MeV for intrinsic germanium detector).

P is subsequently corrected for monitor line detector geometry, gamma ray attenuation in the monitor line wall, and photopeak efficiency of the detector to provide the Bremsstrahlung counts recorded at energy E_p . The programming to carry out these calculations for nonleaking and leaking rods is shown in Fig. 2-41 under the heading "Bremsstrahlung computation."

Compton interaction results from an elastic interaction of a gamma ray with an electron in an absorbing material. When this interaction occurs within the crystal of a gamma ray detector, a scattered photon sometimes escapes without further interactions within the detector. Because some of the energy is carried off by the escaping photon, the electric pulse produced in the detector is less than that associated with the discrete energy of the incident photon. The pulse appears as though a photon of lesser

Fig. 2-41. Revised COUNT program

energy than the incident photon were absorbed and "detected." Many photons of single discrete energy will be scattered through all angles according to the scattering probabilities. Thus, detector pulses from the partially absorbed photons appear to arise from a continuum of lower-energy photons. The ratio of scattered to incident photon energy is calculated from

$$P = \frac{1}{1 + E_0(1 - \cos\theta)/0.510} \quad , \quad (2-4)$$

where $P = E_s/E_0$

= energy of scattered photon/energy of incident photon,

θ = scattering angle (deg).

When Eq. 2-4 is solved for $\theta = 180$ deg (resulting in the smallest possible value of P), P can be substituted into Eq. 2-5 to provide the "Compton edge," i.e., the maximum energy of the scattered electron:

$$E_e = E_0(1 - P) \quad , \quad (2-5)$$

where E_e = energy of scattered electron (MeV).

The total counts at the Compton edge are derived from eq. 2-6:

$$H_{180} = C_0/R_{PC} \quad , \quad (2-6)$$

where H_{180} = total count at Compton edge from a gamma ray of energy E_0 ,

C_0 = photopeak count rate at energy of the incident photon (E_0),

R_{PC} = peak-to-Compton ratio of counts (characteristics of a given detector, assumed equal to 25 for the intrinsic germanium detector used with HELM 3).

To derive the Compton counts at photon energies less than the Compton edge, the electron energy of interest is inserted into Eq. 2-5 as E'_e and the

corresponding value of P obtained. This in turn is substituted into a rearranged version of Eq. 2-4, i.e.,

$$\theta = \arccos \left[1 + \frac{0.510}{E_0} \left(1 - \frac{1}{P} \right) \right] , \quad (2-7)$$

and a value of θ calculated at the selected energy. The probability of a Compton interaction at angle θ is calculated from the expression (Ref. 2-23)

$$p(\theta) = H_{180}(P - P^2 \sin^2 \theta + P^3) , \quad (2-8)$$

where $p(\theta)$ = scattering of photon from electron.

The Compton count rate at E'_e is derived using Eq. 2-9:

$$H = H_{180}[p(\theta)/p(180)] , \quad (2-9)$$

where H = Compton counts at energy E'_e ,

$p(180)$ = scattering of photon from electron at 180 deg.

The program logic for Compton counts from direct gamma emissions is included in Fig. 2-41 under the heading "Compton contribution from direct gamma computation." The Compton counts resulting from the Bremsstrahlung continuum have also been included in the calculation. Equations 2-1 through 2-3 were used except that the number of Bremsstrahlung was calculated in discrete energy bands (ΔE) of 0.020 MeV. The gamma counts in each region of the continuum were then introduced into Eqs. 2-4 through 2-9 to yield the corresponding Compton counts. (This is shown in Fig. 2-41 under the heading "compute Compton contribution from Bremsstrahlung continuum.") Finally, a summation of all counts from direct gamma energy, Bremsstrahlung, and Compton effects provides the total counts at each gamma energy, for each isotope, and for all isotopes.

Table 2-11 summarizes the COUNT calculations for photopeaks of interest in the HELM 3 monitor station tests. Under nonleaking conditions, Bremsstrahlung and Compton effects make no contribution to Xe-133 (81 keV) and Xe-135 (250 keV) signals used to monitor sweep gas activity. This results from the absence of a significant concentration of isotopes having higher-energy beta or gamma emissions. With the onset of a leak, Bremsstrahlung and Compton effects are unimportant until leak rates of $\sim 10^{-2}$ milliliters/s are reached. Even at higher leak rates, the two effects do not compromise the ability to determine leak rates from direct gamma counts at discrete energies of interest. It is worth noting that the gamma counting rates from numerous isotopes at the higher leak rates are partially or totally obscured by Bremsstrahlung and Compton effect, and thus these isotopes were not selected for use in determining the higher leak rates.

A crude check of these calculations has been derived from Ref. 2-24, in which spectra of fission product rare gases are reported after 8 min of decay. In the two instances in which photopeaks of interest in HELM 3 appear in the reported data, i.e., the 258-keV peak of Xe-138 and the 456-keV peak of Xe-137, both measured and calculated results show that background accounts for approximately 10% of the total counts. A more accurate verification of the COUNT code programming will be derived from previous HELM 3 gamma measurements. A Ge(Li) detector has been used to intermittently record gamma spectra in the monitor line under HELM 3 nonleaking conditions. COUNT will be used to simulate the counting geometry and efficiency of the detector to provide calculated emission spectra for direct comparison with measured results. This comparison will provide a check of the programming used to obtain direct gamma counts, but will not be likely to aid in the validation of the Bremsstrahlung and Compton effect programming owing to the low level of activity in the sweep gas. A much better check will result from the HELM test which incorporates a leak of known size into one of the 12-rod fuel bundles.

TABLE 2-11
SUMMARY OF PHOTOPEAK COUNTS FOR ISOTOPEs OF INTEREST
IN HELM 2 MONITOR STATION TESTS

Isotope	Photopeak Energy (keV)	Leak Rate [g/s (millimeters/s)]	Counts/s (% Total)			Total Counts/s
			Discrete Gamma Photons	Bremsstrahlung Continuum	Bremsstrahlung Continuum	
Xe-133	81	0 (0)	85 (0)	<<1 (0)	<1 (0)	86
Xe-135	250		15 (100)	<<1 (0)	<<1 (0)	15
Xe-133	81	10 ⁻⁶ (10 ⁻⁴)	215 (100)	<1 (0)	<1 (0)	216
Xe-135	250		21 (100)	<<1 (0)	<<1 (0)	21
Xe-135	250	10 ⁻⁵ (10 ⁻³)	1110 (100)	<1 (0)	4 (0)	1115
Xe-138	258		<<1 (0)	<1 (0)	4 (100)	4
Xe-138	258	10 ⁻⁴ (10 ⁻²)	814 (93)	<1 (0)	62 (7)	877
Xe-137	456		1 (0)	<<1 (0)	38 (100)	38
Xe-137	456	10 ⁻³ (10 ⁻¹)	928 (81)	2 (0)	222 (19)	1152
Xe-137	456	10 ⁻² (1)	1949 (87)	5 (0)	289 (13)	2243

2.1.2.3.2. Design Basis Transient Testing

The criteria and parameters that determine the cladding faulted temperature limit have been reviewed. The primary criterion is maintenance of a design cooling geometry: no unacceptable fuel assembly deformations and no melting. The three parameters that determine the limit are strength (including irradiation effects), melting, and oxidation (air ingress following the DBDA). Evaluation of these parameters indicates that 1300°C is an appropriate cladding faulted temperature limit. The temperature limit is 75°C below the temperature at which cladding damage would occur.

The results of the evaluation to determine the cladding faulted temperature limit are documented in Ref. 2-23. The purpose of Ref. 2-25 is to obtain Nuclear Regulatory Commission (NRC) acceptance of a GCFR fuel and blanket cladding temperature limit of 1300°C for faulted condition events and to define a testing program to verify the limit. The spectrum of credible plant conditions is identified in accordance with their anticipated frequency of occurrence. These conditions are divided into five categories. The fifth condition is commonly referred to as a faulted condition and corresponds to an expected best-estimate frequency of occurrence per reactor year of 10^{-4} to 10^{-6} . The corresponding off-site dose criterion is 100% that of 10CFR100. The faulted condition shall not preclude achievement and maintenance of safe shutdown and cooldown of the reactor and shall be accommodated without loss of the safety function (for safety systems required to mitigate the consequences of the event) and without exceeding the reactor safety limits. The safety limit is the GCFR fuel and blanket cladding faulted condition temperature limit.

A 1200°C temperature limit is specified by 10CFR50.46 for Zircaloy cladding in light water reactors (LWRs). This limit is based on metal-water reactions, rupture strength, and thermal margins. Similar criteria were used to establish a 1260°C limit for stainless steel cladding in LWRs. A review of the physical parameters affecting the integrity of the GCFR fuel assembly in Ref. 2-25 resulted in selection of a 1300°C faulted condition temperature limit for GCFRs with stainless steel cladding.

The essential requirement for preventing a major radioactivity release is the maintenance of the basic configuration of the reactor core, thus preserving the capability for removal of heat from the core. A gross change of core geometry could reduce or redistribute the coolant flow, resulting in degradation of the cooling capability and damage between adjacent assemblies. Thermal attack of the coolant system boundary by core debris would jeopardize the integrity of the primary system and lead to the release of fission products into the containment building. Major deformations of the core assembly, such as ballooning, melting, or relocation of core material, are therefore considered unacceptable. Changes that preserve the relative arrangement and orientation of the core components, e.g., thermal expansion or fracture of fuel rods, are not unacceptable for the faulted event as long as the coolant flow path is not obstructed and the heat removal capability reduced.

Undercooling of an assembly causes the fuel and stainless steel cladding tubes to heat up. If sufficient heat is removed over a prolonged time, the melting range of stainless steel can be reached. At the solidus temperature of 1410°C , no liquid metal has formed, and the cladding temperature must rise further while absorbing the heat of fusion. With increasing temperature, the fraction of molten material grows. At the liquidus point of about 1440°C , the stainless steel is completely liquid. Somewhere in the range between solidus and liquidus temperature, the cladding material is expected to begin relocating along the cooling channel.

After the liquidus point is reached, liquid metal forms and moves downward on the fuel rod. Because of the axial temperature gradient in the core, relocation of liquid steel brings it into contact with the cladding at a lower temperature, reducing the viscosity of the molten material and slowing down the axial movement. At this location, where marginal movement of liquid steel is possible, molten cladding tends to accumulate, moving slowly downward during core heat-up. Accumulation of material between the fuel rods and the potential for freezing, e.g., in the axial blanket, pose the danger of permanent plugging of the coolant channel and propagation of

the danger of permanent plugging of the coolant channel and propagation of melting in the radial direction. Melting of the stainless steel cladding in the active core or blanket is therefore considered unacceptable for the limiting GCFR faulted event.

Radial expansion of the cladding under internal overpressure would reduce the free coolant flow area in the fuel assembly and the effectiveness of heat removal from the fuel. Because the GCFR fuel rods are vented and kept at the same pressure as the surrounding helium even during a depressurization accident, cladding tube ballooning cannot cause failures in the GCFR design. However, if rod pressurization does occur, only nonductile failure of rods must be considered, because in the GCFR environment the cladding loses its ductility within a few full-power weeks. Nonductile failure is characterized by small cracks through which internal gas can escape; cladding debris is not likely to be released. PES blockages which might form during normal operation would only develop gradually, well beyond the time required for loss of cladding ductility.

Early life failures could only occur if complete blockage due to a manufacturing fault is postulated. Because the rod would be sealed at ambient pressure and temperature, the pressure difference across the cladding following a DBDA would be only about 100 kPa (3 atm). Over the time span during which the cladding loses its ductility, the internal rod pressure could not build up to levels which could cause measurable strain. Thus, in all cases including complete PES blockage, nonductile rod failure would result, with no cladding ballooning and no loss of coolable geometry.

In a core design using structural materials of different composition, diffusion at high temperatures could lead to formation of eutectic alloys. Because of the lowered strength and melting temperature of eutectic alloys, the potential for eutectic alloy formation would pose structural problems. Only stainless steel is utilized in the GCFR core assemblies, and therefore formation of eutectic alloys is not considered possible in the current design.

Extensive experimental investigations have been conducted to determine the tensile strength, ductility, and other mechanical properties of cladding tubes manufactured from 316 stainless steel. However, most of the available data are restricted to the temperature range between ambient and operating temperature. Table 2-12 presents tensile strength data for annealed 316 and 309 stainless steel in the temperature range 260° to 1315°C (Refs. 2-26, 2-27).

The behavior of unirradiated stainless steel cladding under simulated accident conditions has been investigated (Ref. 2-28). Depending on the internal gas pressure and the transient heat-up rate, cladding tubes failed by bursting between 1100° and 1300°C (2000° and 2400°F) at calculated hoop stresses of 44.6 and 9 MPa (6480 and 1300 psi), respectively. The results of these tests (Table 2-13) show that the stainless steel cladding retains some residual strength up to the highest test temperatures. The failure strain of the material decreases with increasing temperatures.

Tests on irradiated specimens similar to those run on unirradiated samples are also reported in Ref. 2-29. The tensile strength and rupture strain of irradiated stainless steel cladding decrease with increasing test temperature, similar to the unirradiated samples. The effect of fast neutron irradiation ($E > 0.1$ MeV) on the failure strain of 316 stainless steel is a strong reduction of the strain at failure caused by the irradiation.

Figure 2-42 portrays the effect of fast neutrons on maximum hoop stress, transient heating rate, and failure temperature. The strength and failure temperatures of 316 stainless steel decrease with fast neutron irradiation. Higher test and irradiation temperatures tend to reduce the effect of irradiation. For irradiated stainless steel the stress at failure decreases with increasing failure temperature, which is similar to unirradiated stainless steel. Neutron fluence, however, appears to have no effect on the strength of 316 stainless steel at very high temperatures [between 1260° and 1370°C (2300° and 2500°F)] (Fig. 2-43).

TABLE 2-12
TENSILE STRENGTH OF ANNEALED STAINLESS STEEL
FROM 260° TO 1315°C (500° TO 2400°F)

Temperature (°F)(a)	Stainless Steel Tensile Strength (ksi)(b)		
	316(c)	316(d)	309(d)
500	73.5	73.5	77.0
700	72.5	72.5	74.0
900	71.0	70.0	69.0
1100	66.5	65.0	61.0
1300	46.0	46.5	45.0
1500	27.5	27.0	25.5
1700	15.0	18.5	15.0
1900	8.0		8.5
2100	3.0		4.5
2300	1.9		2.5
2400			2.0

(a) $1^{\circ}\text{C} = 0.56 (^{\circ}\text{F} - 32)$.

(b) 1 ksi = 689 MPa.

(c) Reference 2-26.

(d) Reference 2-27.

TABLE 2-13
RESULTS OF THERMAL-TRANSIENT TESTS OF UNIRRADIATED 20%
COLD-WORKED 3416 STAINLESS STEEL CLADDING TUBES

Specimen	Hoop Stress (psi)(a)	Initial Test Temperature (°F)(b)	Transient Heat Rate (°F/s)(c)	Diametral Strain (%)	Total Hoop Strain (%)	Termination or Failure Temperature (°F)(b)
4	3240	1110	10	2.5	NA(d)	2200
5	5400	1110	10	13.0	55	2110
6	5400	1110	200	9.0	28	2230
7	3240	1110	200	4.6	8.7	2350
8	3240	1110	200	3.4	NA	2260
9	3600	1350	10	3.3	NA	2250
10	3600	1350	200	2.3	NA	2270
11	6480	1350	10	5.5	61	2120
12	6480	1350	200	19.9	38	2260
13	3240	1110	200	1.3	NA	2050
14	3240	1110	10	1.5	NA	2050
15	5400	1110	10	6.6	NA	2050
16	5400	1110	200	2.1	NA	2050
17	3240	1110	10	7.0	NA	2250
18	3600	1350	10	1.0	NA	2060
19	3600	1350	200	0.8	NA	2030
20	6480	1350	10	11.0	NA	2060
21	6480	1350	200	3.5	NA	2030
23	6480	1350	10	1.2	NA	1900
29	1300	1350	10	0.3	0.7	2350
30	3240	1350	10	2.4	21.6	2300
33	1300	1350	200	0.2	0.3	2420

(a) 1000 psi = 6.89 MPa.

(b) 1°C = 0.56 (°F - 32).

(c) 10°F/s = 5.6°C/s; 200°F/s = 110°C/s.

(d) NA = not applicable.

Fig. 2-42. Effect of irradiation on failure temperature of 20% cold-worked 316 stainless steel cladding during high-temperature transients

Fig. 2-43. Failure strength and strain of irradiated 316 stainless steel at high temperatures (from Ref. 2-29)

Because of the inert coolant, there can be no chemical reaction in the GCFR which jeopardizes the integrity of the fuel assembly. Only in the event of a large leak in the primary system (DBDA), would air ingress introduce oxygen into the reactor coolant system. Although inleakage of air would not start until maximum temperatures have been reached in the system and/or containment equilibrium pressure has been approached, the potential effects of cladding oxidation deserve consideration.

Experimental data on stainless steel oxidation in helium-air mixtures are not available in the literature. Even with the most conservative assumptions concerning air ingress into the GCFR coolant system, the oxygen concentration would be about one order of magnitude lower in the mixture than in air. The low oxygen concentration has two effects: it reduces the oxygen available in the coolant for reaction with the stainless steel cladding and it affects the oxidation rate (Ref. 2-30).

Oxide formation on 304 stainless steel in air and steam atmospheres at temperatures between 1000° and 1370°C (1800° and 2500°F) has been investigated (Ref. 2-31). The parabolic rate constant in dry air was three orders of magnitude lower than that in steam up to about 1370°C (2500°F). Above 1370°C (2500°F) the rate constant in air was about equal to that in steam. Melting of wustite (FeO) at the interphase between oxide and metal at 1375°C (2507°F) was assumed to cause the sudden change observed in the parabolic rate constant. Table 2-14 presents the results of these oxidation experiments.

Based on evaluation of the available data concerning strength, melting, and oxidation of 316 stainless steel, unacceptable conditions in a GCFR fuel assembly are not expected to occur below 1370°C (2500°F). These limits are summarized below.

	<u>Melting</u>	<u>Strength</u>	<u>Oxidation</u>
Temperature limit [°C (°F)]	1400 (2550)	1370 (2550) (estimated)	1375 (2507)

TABLE 2-14
RESULTS OF OXIDATION OF 304L STAINLESS STEEL AT 1000° TO 1375°C
IN STEAM AND IN AIR(a)

Atmosphere	Temperature (°C)	Rate Constants(b)	
		Linear (mg/cm ² -s)	Parabolic (mg ² /cm ⁴)
Steam	1000	0.0030	0.0095
	1050	0.0039	0.030
	1100	0.0087	0.095
	1150	0.016	0.18
	1150	0.017	
	1200	0.033	0.75
	1200	0.034	0.80
	1250	0.038	2.1
	1300		4.4
	1350	0.11	13.0
	1375	(c)	17.0
Air	1100		0.00025
	1200		0.00005
	1225		0.00012
	1350		0.0074
	1360		0.0079
	1375		21.0
	1375		25.0

(a) Dry air (66°C dew point).

(b) For tests in steam there was a change from linear to parabolic behavior after times ranging from 6 to 28 min.

(c) Not observed.

It can be concluded that unacceptable conditions in a GCFR fuel assembly do not occur below a temperature of 1370°C (2500°F). This is in good agreement with the fuel damage limit for other reactor concepts utilizing stainless steel cladding. In the Clinch River breeder reactor (CRBR), the limit for coolable geometry is defined as 1370° ± 15°C (2500° ± 25°F) (Ref. 2-32), the corresponding limit for the British advanced gas-cooled reactor (AGR) is 1350°C (2462°F). Both concepts have nonvented fuel rods.

While the experimental programs are in progress, an additional safety margin of 70°C (130°F) has been applied to provide a conservative faulted temperature limit, resulting in a maximum allowable cladding temperature of 1300°C (2370°F). Unacceptable fuel conditions are not anticipated below 1370°C (2500°F), and there is a high degree of confidence that this limit will be verified by the experimental programs in progress. However, it is consistent with a generally conservative approach to provide a margin before the conclusion of the verification program. The selected limit is in good agreement with the fuel damage limits of other reactor concepts utilizing stainless steel cladding:

	AGR (Ref. 2-33)	CRBR (Ref. 2-32)	GBR (Ref. 2-34)
Faulted temperature limit [°C(°F)]	1350 (2462)	1370 ± 15 (2500 ± 25)	1300 (2370)

Thus, the conservative value for the faulted temperature limit of the GCFR fuel and blanket cladding is 1300° (2370°F).

To broaden the data base for the faulted limit of the GCFR fuel assembly, an experimental program to study the mechanical, physical, and thermal properties of 316 stainless steel has been started. The resulting data will increase the accuracy of fuel assembly performance prediction and

improve the understanding of the limiting phenomena that determine the faulted temperature limit of the fuel assembly. The program includes

1. Material property tests on irradiated and unirradiated samples at temperatures approaching the cladding melting temperature.
2. Laboratory tests of the structural response characteristics at temperature of cladding subjected to the loadings during a DBDA.
3. Electrically heated rod bundle tests to determine the composite bundle effects during simulated DBDA conditions. These tests include the DACT and CFTL.
4. In-pile tests of irradiated rod bundles subjected to simulated DBDA conditions.

There is a high probability that the verification test program will confirm the 1300°C (2370°F) limit. The margin in this limit is consistent with the level of sophistication of the verification test program and the level of confidence in the evaluation models used to calculate accident temperatures.

2.1.2.3.3. Thermal-Hydraulic Methods Development

The COBRA*GCFR code (Refs. 2-35, 2-36) is a modified version of the COBRA-IV code (Ref. 2-37) adapted for gas-cooled assemblies. Further modifications to COBRA*GCFR are necessary for correct laminar flow analysis (Ref. 2-38). Hence, several modifications have been incorporated in the COBRA*GCFR code, and a review of the COBRA transient method was initiated.

The entrance effect on the laminar heat transfer coefficient is described by (Refs. 2-39, 2-40)

$$St = St_{\infty} \left[1 + \frac{1.99}{Z^{0.506} \times e^{0.041Z}} \right], \quad (2-10)$$

where St_x = Stanton number at location x ,

St_∞ = Stanton number for fully developed conditions,

$Z = 1/Gz \times 10^3$,

Gz = Graetz number

$$= \frac{Re_B Pr}{x/de} ,$$

x = distance from beginning of heating.

Equation 2-10 has been programmed in the COBRA*GCFR code. The entrance effect is applied to the heat transfer coefficient whenever the Reynolds number is less than 2000.

The conduction model described in Ref. 3-27 was incorporated in the COBRA*GCFR code and modified as follows:

1. Momentum, energy, and continuity equations are solved to calculate flow distribution, heat transfer coefficients, and coolant temperatures in all channels at axial level I.
2. The circumferential rod conduction subroutine CIRCON uses the calculated parameters in the previous step as boundary conditions and calculates the temperature distribution in the rod and the surface heat fluxes for each channel at level I.
3. Momentum, energy, and continuity equations at axial level I are resolved with the use of local heat flux values.
4. The procedure is repeated until convergence is reached at axial level I; then the calculations proceed to axial level I + 1.

Although the procedure described above requires slightly larger computer storage than before, previously encountered convergence problems were eliminated.

Reference 2-41 describes a radiation model which uses radiosities.* COBRA uses considerable computer core storage for this procedure, and hence an alternate procedure with Hottel's total radiation exchange factors (Ref. 2-42) was adapted in the COBRA*GCFR code. Hottel's total radiation exchange factors are calculated using the view factors and emissivities of all surfaces in the procedure described in Refs. 2-42 and 2-43. The input to COBRA*GCFR consists of pairs of surfaces with the associated Hottel's total radiation exchange factor.

The two numerical methods used in the COBRA-IV code for transient calculations, viz, "implicit" and "explicit," were reviewed. The implicit scheme can be used only if there is no flow reversal and the axial flow is greater than the lateral flow. The explicit scheme can handle flow reversal, but is limited to small time steps for numerical stability. The explicit scheme has been successfully used to calculate the transient problem with water coolant provided in Ref. 2-38. To run a gas-cooled problem with the explicit scheme, a four-rod, four-channel model of the edge of a GCFR fuel assembly was constructed. However, because of the relatively high velocity under the Courant condition, the time step must be small.

Using a time step of 0.0003 s, the sample problem can be successfully carried out to about 0.007 s in 318 s of computer execution time. The helium velocity in this typical problem is about 4 m/s. This is consistent with the water-cooled problem, which has a velocity of 0.12 m/s, and is stable with a time step of 0.01. This practical operating condition of relatively high gas velocity makes the time step limit extremely small. The possibility of modifying the current method so that the time step requirements can be relaxed is being investigated.

At present, two methods are available for determining hot spot factors: CRBRP worst value analysis (Ref. 2-44) and PACT-2 analysis using Monte Carlo techniques (Ref. 2-45). In the worst value analysis, COBRA-IV

*The rate at which radiation leaves a given surface per unit area.

is used for channel and film factors to take advantage of intersubchannel coolant mixing. These subfactors are divided into two groups. Those occurring randomly, e.g., manufacturing and assembling tolerances, properties of materials and coolant, and correlations, are listed as statistical subfactors (f_{s_i}). The nonrandom components (uncertainties in calculations and measurements) are added directly as cumulative subfactors (f_{c_i}).

The statistiscal subfactors are combined to give a total statistical factor (f_{s_a}):

$$f_{s_a} = \sqrt{\sum_i (f_{s_i} - 1)^2 + 1} \quad , \quad (2-11)$$

and the total cumulative subfactor (f_{c_a}) is given by

$$f_{c_a} = \prod_i f_{c_i} \quad . \quad (2-12)$$

The overall hot spot factor (F_a) for component is then defined as

$$F_a = f_{s_a} \times f_{c_a} \quad . \quad (2-13)$$

The PACT-2 analysis using Monte Carlo techniques computes the probability distributions of the coolant and cladding temperatures throughout a single assembly during steady-state operation. It uses a model that considers both thermal-hydraulics in coolant subchannels and Monte Carlo sampling of the thermal-hydraulic calculation. In addition to calculating the hot spot subfactors, PACT-2 generates cladding and coolant temperature histograms for any axial elevation and gives a printout of the nominal temperature and standard deviation for coolant and cladding at any axial elevation. Additional studies and modifications are required so that this code can be used for GCFR hot spot factors and probabilistic analysis.

Both methods of determining hot spot factors give nominal temperatures and standard deviations of each rod in a fuel assembly. The worst value method is more conservative than the Monte Carlo technique, but neither

directly gives the probability of any rod in an assembly exceeding 700°C nor the number of rods in the reactor that exceed 700°C. The probability P_i of each rod exceeding 700°C can be determined from the data supplied by either method. If there are n rods in a subassembly and N rods in a core, then the probability $P(A)$ of exceeding 700°C in the assembly is given by

$$P(A) = \prod_{i=1}^n [P_i(700)] \quad , \quad (2-14)$$

and the probability $P(C)$ of exceeding 700°C in the core is given by

$$P(C) = \prod_{i=1}^N [P_i(700)] \quad . \quad (2-15)$$

This can become a long and tedious process since there are about 40,000 fuel rods and 14,600 blanket rods in a 400-MW(e) reactor core.

The statistical analysis code SHOSPA (Ref. 2-46) calculates Eqs. 2-14 and 2-15 in addition to determining the hot spot factors. The hot spot factors are calculated by a method similar to the worst value method, but with a single subchannel, so that no advantage can be taken of coolant mixing between subchannels. Using data on core geometry and operating conditions in different zones of the core, SHOSPA then proceeds to calculate the following:

1. The margin from nominal to critical temperatures in terms of hot spot factors.
2. The probability of exceeding the critical temperature.
3. The probability of at least one hot spot in the core.
4. The probability of exactly n hot spots in the core (where $n = 1$ to 10).

Modifications have to be made to SHOSPA and PACT-2 so that they can be applied to GCFR designs. SHOSPA will thus supply data on the confidence level of the assemblies for the core, and PACT-2 can be used to reduce the hot spot factors from their present values.

During the operation of a reactor, the only operating conditions that are directly measurable are the inlet and exit mixed mean temperatures of the core assemblies. With this information and the corresponding physics data on power level and power profiles, it is possible to use EXTEMP (Ref. 2-47) as a quick method of determining the peak hot spot temperature in an assembly. If this peak temperature exceeds a given critical temperature (700°C at the midwall of the GCFR), a new power level is calculated at which the assembly can operate. Figure 2-44 was generated using this capability of the program. The flow axis is representative of $1/(T_{out} - T_{in})$, which is a measured quantity. The figure defines the upper operating boundary of a fuel assembly. Any operating condition has to lie on or to the left of the critical temperature line (700°C).

2.1.3. Core Design Verification and Support

2.1.3.1. Test Programs.

2.1.3.1.1. Critical Assembly Experiments

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

Calculations have been completed on the experiments done during the phase 3 GCFR critical assembly using methods routinely employed for design of GCFR power plant cores and for performance predictions. In general,

Fig. 2-44. Critical temperature line for fuel assembly

there was good agreement between calculated and measured phase 3 parameters, and hence the physics methods used have been substantiated. The results of these calculations, which used the standard 10-group cross section structure for the GCFR core design analysis, agree well with results of ANL calculations using a more rigorous 29-group structure.

The basic nuclear data (ENDF/B-4) and the methodologies used still yield biases of calculated predictions relative to measured results. With regard to criticality determinations, the calculations yield eigenvalues which are a factor of 0.986 times the experimental k assessments for the ZPR critical configurations (a bias of $-1.4\% \Delta k/k$). The central worth discrepancy continues to appear, but with a calculated/experimental (C/E) factor of 1.12 for plutonium, which is somewhat smaller than that obtained in previous GCFR assemblies and smaller by about 4% than the results of ANL worth calculations. Table 2-15 compares the measured and calculated worths for small samples of fissile and other core materials at the center of the phase 3 GCFR benchmark assembly. Radial worth distributions were also measured, and Figs. 2-45 and 2-46 compare GA-calculated worth profiles with experimental values for samples of plutonium and U-238. For Pu-239, the calculated radial worth distribution agrees quite well with the measured data after normalization of the central measurement. This agreement is typical for highly fissionable and highly absorbing materials. For other materials, however, the calculated (normalized) profiles deviate from measured plots, as seen in the U-238 results in Fig. 2-46.

There is still a sizable discrepancy between the calculated and measured breeding ratio parameters. The U-238 capture to Pu-239 fission rate ratios in the cell fuel are calculated to be 13% greater than ratios derived from in-plate foil irradiation experiments. However, these measurements are in conflict with the results of a somewhat less rigorous experiment which determines the isotopic capture-to-fission ratio for U-238. This disagreement highlights the need for (1) reexamination of the experimental technique used for the U-238 capture to Pu-239 fission ratio and (2) definitive intercalibration with radiochemical techniques.

TABLE 2-15

COMPARISON OF MEASURED AND CALCULATED MATERIAL SAMPLE WORTHS
AT CORE CENTER OF PHASE 3 GCFR CRITICAL ASSEMBLY

Material	Sample(a)	Principal Isotope (wt %)	Sample Worth(b) (Ih/kg)		C/E
			Calculated	Measured	
Fissile	MB10	Pu-239 (96.9)	158.7	141.7 $\pm$ 1.3	1.120 $\pm$ 0.010
	U233-3	U-233 (99.0)	215.9	190 $\pm$ 4	1.136 $\pm$ 0.024
	MB21	U-235 (93.2)	119.6	103.3 $\pm$ 0.9	1.158 $\pm$ 0.010
Fertile	MB25	U-238 (99.8)	-8.283	-7.63 $\pm$ 0.32	1.086 $\pm$ 0.046
	PU240-2D	²⁴⁰ PuO ₂ (80.7 Pu-240)	+19.32	17.1 $\pm$ 0.3	1.130 $\pm$ 0.020
Poison	B-7	B-10 (87.1)	-1948	-1928 $\pm$ 40	1.010 $\pm$ 0.021
	LI-5	Li-6 (99.1)	-1696	-1502 $\pm$ 20	1.129 $\pm$ 0.015
Structural	FE-1	Fe (100.0)	-5.138	-3.85 $\pm$ 0.34	1.335 $\pm$ 0.121
	MN-1	Mn (100.0)	-13.360	-7.27 $\pm$ 0.63	1.838 $\pm$ 0.159
	MB19	Steel (type 304)	-5.688	-4.68 $\pm$ 0.07	1.215 $\pm$ 0.020
Moderator	CH ₂ (c)	CH ₂ (foam)	-222	+8.8	-25
	POLY-1	CH ₂ (solid)	-222	+155 $\pm$ 4	-1.43
	H2O-1A	H ₂ O (in capsule)	-169	+108 $\pm$ 3	-1.56

(a) As solid or annular cylinders with up to a 2.1-cm diameter and a 5.5-cm length.

(b) Calculated by first-order perturbation theory for a mesh interval centered at $r = 1.05$ cm, $z = 5.09$ cm.

(c) Measured within traverse guide tube which is axially offset 4.4 cm from the core midplane at the center of the central matrix tube.

Fig. 2-45. Radial worth traverse for Pu-239 in phase 3 critical assembly

Fig. 2-46. Radial worth traverse for depleted uranium in phase 3 critical assembly

Calculated and measured fission and capture rate spatial distributions are in good agreement, indicating that the power density profiles in the core can be calculated to an accuracy of about 3% (which is the deviation at the core/blanket interface with central normalization). Figures 2-47 and 2-48 compare the radial and axial fission rate profiles calculated for Pu-239 with the corresponding plots of measured relative rates. The calculated radial profile is the same as the measured profile within 3%; however, some discrepancies are noted for the axial distribution calculations. This suggests slight underprediction of the axial leakage parameters as defined by streaming effect corrections. The diffusion modifiers for the direction perpendicular to the plate alignments appear to produce discrepancies in calculated horizontal reaction distributions. Higher leakage terms could improve the flux and reaction rate profile calculations, but would also increase the eigenvalue bias to approximately -1.8%.

A discrepancy in the rod-for-plate fuel cell substitution worth prediction indicates that further examination of heterogeneity and streaming effect accounting is needed. The rod zone experiment indicated that the calculated eigenvalue of a critical rodded fuel reactor would be somewhat higher than the 0.986 factor obtained for the reference phase 3 plate configuration. The consistency of the methods used for these studies and those used for rodded fuel assembly core calculations must be verified so that the eigenvalue bias of the GCFR design calculations can be assessed.

The completed analysis covers all the experimental results documented in for phase 33 in Ref. 2-48 except the steam worth experiment in the rod zone (requiring 28-group analysis). Complete details of the analysis and the results are given in Ref. 2-49.

2.1.3.1.2. Shielding Experiments

The purpose of this task is to verify the adequacy of the analytical methods and data for the nuclear design of the GCFR shielding. The task

Fig. 2-47. Radial fission rate profile of Pu-239 in phase 3 critical assembly

Fig. 2-48. Axial fission rate distribution of Pu-239 in phase 3 critical assembly

coordinates and provides liaison with the experimental shielding program at ORNL. Specific work tasks include

1. Preparation of objectives and requirements for experiments to verify methods and data for shielding nuclear design.
2. In conjunction with ORNL, definition of experimental configurations and measurements to ensure methods and data verification. Preanalysis and postanalysis will be performed as necessary.
3. Assurance that the experimental program schedule is compatible with the overall GCFR program schedule.
4. Application of experimental methods and data and bias factors to the shielding design.

During this quarter, revised alternative inner radial shield configurations for the GCFR radial blanket and shield experiments were provided to ORNL, and all priority 1 measurements were completed at the ORNL lower shield facility (TSF). In addition, plenum streaming experiment requirements were given to ORNL (Ref. 2-50). The plenum streaming experiment will comprise four phases, which will be conducted during FY 80 and 81.

2.1.3.1.2.1. GCFR Radial Blanket and Shielding Experiment. The inner and outer radial shield structures are part of the high-temperature components located in the upper reactor plenum. The uncertainties in analysis of neutron and photon transport in this region can be grouped into two broad categories:

1. Uncertainties in deep penetration transport through successive thicknesses of blanket, shielding materials, and PCRV concrete.
2. Uncertainties in neutron and gamma ray streaming in geometrically complex helium channels and plenums.

The radial shield experiment is concerned with the deep penetration uncertainties; the plenum steaming experiment addresses the streaming uncertainties. These uncertainties will be investigated separately because a nearly full-scale mock-up of the core and blanket source and the lower reactor plenum would be required for concurrent investigation of both uncertainties. The radial shield experiment can be achieved by a slab mock-up which simulates the successive material layers at the core midplane axial level. The specific objective of the radial shield experiment is to verify methods and data for calculating

1. The energy dependence of the neutron and gamma ray fluxes through the radial blanket, radial shielding, and PCRV.
2. Gamma ray production and heating in the radial blanket (uranium and thorium), radial shielding, and PCRV.
3. The heterogeneous effects resulting from small gaps and manufacturing/construction tolerances in the as-built radial shield components.

Configuration uncertainties at the start of the experiment (e.g., upflow versus downflow, number of rows, and blanket or shield assemblies) required that the measurements have the flexibility to be changed and that additional configurations be included.

The radial shielding experiment must provide (1) methods and data benchmark problems for the experimental environment and (2) bias factors for radiation transmission, heating, and the spectrum to be applied in actual design calculations. The benchmark problems enable comparison of methods and data used in design analyses at GA and ORNL and provide a vehicle for developing and/or checking new methods and data. Sensitivity analysis can be used to determine sources of errors to yield the bias factors. The confidence in extrapolating experimental results to obtain bias factors for use in design analyses is enhanced when the experimental environment is prototypical of the reactor environment.

The radial shield experiment consists of three phases:

1. Blanket simulation phase.
 - a. Thorium blanket.
 - b. Uranium blanket.
2. Shield simulation phase.
 - a. Reference inner radial shielding configuration.
 - b. Reference outer radial shielding configuration.
 - c. Alternative inner radial shielding configuration.
 - d. Shield heterogeneities.
3. PCRV simulation phase.

Each phase can be performed independent of the others (i.e., a different spectrum modifier can be used in each phase to modify the TSR-II source beam and approximate the incident source at the core-blanket interface, blanket-shield interface, and shield-PCRV interface in the actual reactor environment).

Table 2-16 lists the configurations and measurements needed for each experimental phase. Because the funding is not sufficient for performing all the measurements, the measurement priorities shown in Table 2-16 were determined. As planned, all priority 1 measurements (i.e., phases 1a, 1b, 2a, 2b, and 2c) were completed. A schedule for completion of phases 2d and 3 has not yet been established, but will be factored into the overall program with the plenum streaming experiment.

Table 2-17 shows the modifications to the phase 2c measurements which were incorporated (Ref. 2-51) to reflect

1. The upflow core decision.
2. The change in reflector/shield assembly design.
3. The core restraint cylinder.

TABLE 2-16
EXPERIMENTAL CONFIGURATION FOR GCFR RADIAL BLANKET
AND SHIELDING EXPERIMENT

Configuration	Description	Priority(a)				Phase
		1	2	3	4	
1	Spectrum modifier	BB, S				1a
2	1/2 thorium blanket row	BB				
3	1 thorium blanket row	BB				
5	2 thorium blanket rows	BB, S, TLD				
6	3 thorium blanket rows	BB, S, TLD				
6	3 thorium blanket rows + carbon steel reflector	TLD				
7	3 uranium blanket rows	BB, S, TLD				1b
8	3 uranium blanket rows + inner shield	BB, S				
9	Row 1 inner shield	BB, S				2a
10	Row 1 and 2 inner shield	BB, S				
11	Inner shield + 1/3 outer shield	BB				2b
12	Inner shield + 2/3 outer shield	BB				
13	Inner + outer shields	BB, S				
14	Inner + outer shields + PCRV	BB, TLD				
15	Graphite row 1 + stainless steel row 2 inner shields	BB, TLD		S		2c
16	Stainless steel row 1 and 2 inner shields	BB, TLD		S		
17	Stainless steel row 1 + (C + B ₄ C) row 2 + stainless steel row 3	BB, TLD		S		
18	15.24 cm C + B ₄ C 5.08 cm stainless steel		HB			2d
19	15.24 cm C + B ₄ C 2.54 cm stainless steel		HB			
20	15.24 cm C + B ₄ C 0.63-cm gap		HB			
21	15.24 cm C + B ₄ C 1.27-cm gap		HB			
22	15.24 cm stainless steel no gap		HB			
23	15.24 cm stainless steel 0.63-cm gap		HB			
24	15.24 cm stainless steel 1.27-cm gap		HB			
25	15.24 cm stainless steel 2.54 cm gap		HB			
26	Carbon steel + 45-cm PCRV				BB, S	3
27	Carbon steel + 45- and 30-cm PCRV				BB, TLD, S	

(a) BB = Bonner ball, S = spectrum; TLD = thermoluminescent dosimeter; HB = Hornyak button.

TABLE 2-17
ALTERNATIVE INNER SHIELD CONFIGURATIONS

Reflector Shield			Core Restraint Cylinder
Row 1	Row 2	Row 3	
Graphite	--	--	--
Graphite	B ₄ C + C	--	4.45-cm stainless steel
Graphite	B ₄ C + C	Stainless steel	4.45-cm stainless steel
B ₄ C + C	--	--	4.45-cm stainless steel

2.1.3.1.2.2. GCFR Plenum Streaming Experiment. The GCFR plenum streaming experiment requirements have been established (Ref. 2-52). Reference 2-52 is a working document which will be revised as shielding design and experiment planning progresses. It will also provide guidance for final specifications to be prepared by ORNL.

The shielding, structures, and ducts comprising the upper and lower plenums in the GCFR are regions of geometric complexity. The uncertainties in computing the component exposure levels in the GCFR plenums can be separated into four categories:

1. Uncertainties in streaming in the coolant channels between the core and blanket rods and fuel assemblies and streaming in the grid plate and exit shield regions.
2. Uncertainties in neutron and gamma ray streaming in the geometrically complex and sometimes large helium ducts, channels, and plenums.
3. Uncertainties in deep penetration transport through successive thicknesses of laminated shielding and PCRV concrete.
4. Uncertainties in transport through the laminated shielding due to the complex helium flow channels and manufacturing/construction gaps and tolerances.

Since streaming within the core and core shielding affects the sources of leakage into the plenums, and plenum streaming affects the source incident on the large shield structures, uncertainties in each category become additive. The GCFR grid plate shield design confirmation experiment was concerned with core and grid plate streaming, and the GCFR radial blanket and shielding experiment addressed deep penetration transport. The overall objective of the plenum streaming experiment is to investigate exit shield transport and plenum streaming uncertainties including the heterogeneous nature of the plenum shields. The experiment has been divided into four

phases to address four areas of uncertainty associated with calculating transport from the core to the plenums and ultimately to the PCRV (i.e., exit shield streaming, plenum streaming, shield heterogeneities, and prototypical plenum shield simulation). An experiment which simultaneously addressed all uncertainties was precluded because a nearly full-scale mock-up of the core plenums and ducts would have been required.

The shield heterogeneity is concerned with uncertainties associated with

1. Helium gaps within and between shield assembly components which result from manufacturing and construction tolerances, thermal expansion, and radiation-induced dimensional changes in the shield materials.
2. Heterogeneities resulting from the steel encasement of the graphite and boronated graphite shield materials (e.g., enhanced transport in the low cross section windows of steel).

The basic test section arrangement is identical to the slab arrangement used for the radial shield experiment except that the next to the last slab consists of the heterogeneity mock-up (Fig. 2-49). This would be followed by a steel/concrete slab to simulate PCRV liner and reflector effects.

Only generic mock-ups of helium and/or steel gaps are required to validate transport methods for the expected range of gaps. Measurements are expected to provide guidance on the tolerances allowed during fabrication and assembly of the shielding assemblies. These measurements would be limited to high spatial resolution measurements of flux peaking from Hornyak button detector traverses perpendicular to the gaps and global measurements of total flux with various size Bonner balls.

The exit shield phase addresses the combined effects of streaming in the core, axial blankets, and fuel assembly exit shields. The proposed experimental configuration (Fig. 2-50) is identical to that used for the GCFR grid plate design confirmation experiment except that the grid plate

Fig. 2-49. Schematic of shield heterogeneity measurements

Fig. 2-50. Experimental configuration for exit shield simulation phase

shield mock-ups are replaced with exit shield mock-ups. The only test sections which must be fabricated are the seven steel ducts containing the prototypical exit shield components.

It may not be necessary to consider fuel rod pitch and interassembly duct gap spacing as parameters, since the grid plate shield experiment provides sufficient data to enable interpolation over the range of expected core parameters. The 91-rod/assembly and 12.7-mm duct gap configuration best approximates the current design for the 400-MW(e) two-loop GCFR demonstration plant.

The grid plate shield design confirmation experiment emphasized measurement of local grid plate exposure, and the measurements stressed determination of local flux peaking by traverse measurements with small Hornyak button scintillation detectors. The types of measurements emphasized in the exit shield phase are those which characterize the global behavior of the transmitted flux (i.e., the angular and energy dependence of the source to the upper plenum). Proper characterization of the transmitted flux will probably require distant measurements to determine the angular dependence. The grid plate shield experiment has enabled specification of required configurations and measurements:

1. Spectrum modifier.
2. Seven-fuel-assembly bundle.
3. Seven-fuel-assembly bundle followed by exit shields.

The objective of the plenum streaming phase is to quantify the uncertainties in computing radiation transport in the large upper and lower plenums, where mean free paths are very long. This will probably be the most difficult phase of the experiment, and it is anticipated that considerable preanalysis will be required to define the test section configuration. The criteria for the test section include the following:

1. The cavity mock-ups (upper and lower) must be sufficiently large to exhibit the gross transport calculation difficulties associated with large plenums (e.g., ray effects).

2. Because plenum streaming is primarily a geometry effect (except for reflection from plenum walls), the angular dependence of the source into the cavity must be well known.
3. The radial variation of the source into the cavity must be minimized or at least well defined.

The plenum shield simulation phase addresses uncertainties in computing transport through the as-built laminated shielding in the plenums and into the PCRV concrete. Prototypical mock-ups of the plenum shielding structures, including the helium flow passage, are required. Figure 2-51 shows the probable experimental configuration. The design of the shielding in the plenums will not be firm until sometime prior to completion of the phase I conceptual design, and thus it is not yet possible to define the heterogeneous shield mock-ups which will be required or the parameters (e.g., materials, gaps) which might need to be varied to cover configuration uncertainties. If the conceptual shield design includes laminated shielding with helium flow-through passages (for free convection requirements), high resolution measurements of local flux peaking will be essential.

2.1.3.2. Methods Development. MICROX-2, an improved version of the MICROX code (Ref. 2-3) is an integral transport theory flux spectrum code which solves the neutron slowing-down and thermal energy range equations on a detailed energy grid for a two-region lattice cell. The fluxes in the two regions are coupled by collision probabilities. The computation of these collision probabilities is based upon the spatially flat neutron emission approximation and the transport (modified P_0) approximation of anisotropic scattering effects. The collision probability calculation also uses an energy-dependent Dancoff correction factor algorithm. A second level of heterogeneity can be treated, i.e., the inner region may include two different types of grains (particles). Neutron leakage effects are treated by performing B_1 slowing-down theory (fast and resonance energy ranges) or P_0 plus DB^2 (thermal energy range) calculations in each spatial region. Energy-dependent bucklings (positive, zero, or negative) are supplied as

Fig. 2-51. Experimental configuration for plenum shield simulation phase

input. To reduce execution time and simplify input preparation, spectrum and broad-group cross section calculations for several different two-region lattice cells may be performed in parallel during a single computer run.

The basic nuclear data used by MICROX-2 consists of the GAM data (Ref. 2-53) available for 99 fine groups covering the fast energy range 14.9 MeV to 0.414 eV, the GAR data (Refs. 2-54, 2-55) available for 14,500 discrete energies in the resonance energy range 8 keV to 0.414 eV, and the GATHER data (Ref. 2-52) available for 101 discrete energies in the thermal energy range 2.38 to 0.001 eV. The MICROX-2 code can explicitly account for overlap and interference between all resonance levels in both the resonance and thermal energy ranges and allows for simultaneous treatment of leakage and resonance self-shielding in doubly heterogeneous lattice cells. Resonance self-shielding above the energy range covered by GAR data is performed by interpolation between sets of temperature- and dilution-dependent GAM fine-group data prepared by the GFE code (Ref. 2-55).

Detailed neutron flux and current spectra are used for condensing the basic neutron cross section data into cell-averaged, broad-group cross sections and scattering transfer arrays (up to P_1). Broad-group P_2 and P_3 scattering transfer arrays may be determined with P_2 and P_3 weighting spectra obtained from an extended transport approximation (Ref. 2-56) (fast and resonance energy ranges only). The broad-group cross sections produced by MICROX-2 may be used in either diffusion or transport theory codes. MICROX-2 includes new algorithms which utilize the hyperfine mesh fluxes and currents and the Benoist lattice cell homogenization prescription (Ref. 2-57) for preparation of more accurate transport cross sections for use in diffusion theory codes. The treatment of leakage in few-group transport theory calculations has been improved with the incorporation of the "modified P_n " scheme of Ref. 2-58 in a more rigorous form than in the original MICROX code.

The fast energy range B_1 approximation slowing-down calculations in MICROX-2 use explicit GAM-type fine-group scattering transfer arrays and are essentially the same as the B_1 options in the GGC-4 and GGC-5 codes

(Refs. 2-4, 2-59), except that two spatial regions are treated and the format of the fine-group data tape has been extensively modified to minimize the execution time and memory requirements.

The resonance energy range calculations use a new version of the high-speed synthetic scattering kernel slowing-down algorithms introduced in the original MICROX code. The new resonance region slowing-down algorithms are more accurate in highly absorbing systems such as fast breeder reactors and allow the use of a true B_1 approximation in each spatial region.

The presence of up-scattering in the thermal energy range necessitates the use of an iterative solution of the neutron balance equations. MICROX-2 starts with a Maxwellian flux guess joined smoothly to a $1/E$ higher-energy component at $E = 10$ kT and then uses a combination of Gauss-Seidel (successive substitution) iteration (from high to low energies), renormalization, over-relaxation, and few-group rebalancing to accelerate convergence. Convergence is usually obtained in five to seven iterations, even for hard-spectrum systems for which the original MICROX thermal region algorithms failed to converge. Leakage in the thermal energy range is treated by the inclusion of DB^2 terms into P_0 balance equations. The thermal neutron current needed for the preparation of transport cross sections and P_1 scattering transfer arrays is obtained from the $J = D|B|\phi$ approximation. The resonance region calculations include an approximate correction for finite temperature effects using a new slowing-down approach so that the slowing-down source into the thermal region is more accurate than would be obtained from the zero temperature elastic slowing-down kernel approach usually used in the resonance region.

MICROX-2 can calculate broad-group cross sections and scattering transfer arrays at arbitrary user-specified temperatures by interpolation between sets of temperature-dependent GAM, GAR, and GATHER input. The user may specify arbitrary combinations of fission spectra for various fissionable isotopes in each spatial region. MICROX-2 is suitable for a wider range of uses than the original MICROX code (which was specifically developed for analysis of HTGRs). Although there is some loss of speed in

MICROX-2 compared with the original MICROX code, careful attention to efficiency in algorithms and data management in MICROX-2 has resulted in a code that is considerably more efficient, more rigorous, and easier to use than the previous general purpose GGC-5 code (Ref. 2-4).

2.1.3.2.1. Core Performance Thermal-Hydraulic Methods Development

A number of revisions were made to the CALIOP (Ref. 2-60) reactor design program to improve modeling, extend capability, and shorten running time. Changes were included which allow for fissile buildup and burnout in the blanket and provide the means for an equilibrium cycle calculation. A preliminary calculation is made with fresh fuel and no blanket fissile material. The reactivity is calculated at the end of the cycle (EOC), including the effect of fission products and blanket fissile material. The fission products are then corrected to the beginning of equilibrium cycle (BOEC) value as a function of the core reprocessing rate. The blanket fissile content is adjusted to the average equilibrium cycle value as a function of blanket reprocessing rate, and the fuel fraction of theoretical density is reduced in proportion to the unfissioned fraction at BOEC. The initial reactivity is then adjusted to give the desired EOC value, and the run is repeated. The second run gives fuel enrichment and breeding ratio at BOEC. The breeding ratio includes the effect of fission burnout in the blankets.

The cross section subroutine XGEN has a set of built-in one-group plutonium and uranium cross sections. A new option enables these cross sections to be input, using multigroup calculations from a code such as 2-DB as a source for the same approximate geometry. Coding will be provided within CALIOP to produce these cross sections as a function of core contact and size.

The criticality subroutine VFRAC was originally written to calculate the critical fuel volume fraction. First it calculates the initial constant k and then iterates fuel volume fraction to get the desired input values of k . Since it is used to find critical enrichment rather than

volume fraction, the volume fraction iteration was eliminated and enrichment was directly iterated for the desired reactivity. This greatly shortened the running time.

The cladding and duct material may have different structural requirements, and therefore different materials may be necessary. Cladding must withstand the highest temperatures in the core. Duct material is at lower temperatures, but must resist deformation from stress and swelling. Hence, the coding was revised to permit separate specifications for cladding and duct materials.

Detailed multichannel thermohydraulic calculations, using the COBRA code indicate that the proper hot channel factor to use in a single-channel code is a function of the flux tilt across the channel. The recommended relation was inserted in subroutine CHANEL.

It has been noted that a core with variable-enrichment zoning may still have channels which require orifices. The performance of such a core could obviously be improved by enlarging the rods in the channels with orifices to the point where maximum cladding temperatures would be reached without orifices. To explore this possibility, an option was provided wherein the core is simultaneously designed with up to four zones of different enrichment and individually sized rods in each channel. This improved the design of a conventional core and made it possible to study, for example, a completely ductless core with batch refueling of the entire core and blankets.

The thermohydraulic core calculations proceed progressively channel by channel and it was noted that total core power could be calculated after only the first channel was completed. Therefore, the first iteration on rod pitch was moved to this point, rather than going through all the channels first. This greatly reduced the code running time.

2.1.3.2.2. Rod Thermal-Mechanical Analysis

2.1.3.2.2.1. Gas-Cooled Version of LIFE-IV Code. LIFE-IV, an LMFBR fuel rod thermomechanical code (Ref. 2-61), has been modified for application to the GCFR. The modification was essentially accomplished by changing the coolant heat transfer formulation in LIFE-IV from liquid sodium to gaseous helium. The helium film heat transfer coefficient for the rough section is determined from the empirical Nusselt number (Nu) correlation (Ref. 2-62):

$$\text{Nu} = 0.0161 (\text{Re})^{0.863} , \quad (2-16)$$

where Re is Reynolds number calculated at coolant bulk temperature. Equation 2-16 represents a slight revision of

$$\text{Nu} = 0.0138 (\text{Re})^{0.878} , \quad (2-17)$$

which was used in the gas-cooled version of LIFE-III (Ref. 2-63). The Nusselt number correlation for the smooth section remains unchanged from that used in the gas-cooled version of LIFE-III. In addition to the change of coolant heat transfer formulation, the cladding o.d. wastage is ignored. Unlike liquid sodium, helium is not likely to significantly corrode the cladding under GCFR operating conditions (Ref. 2-64).

The LIFE-IV gas-cooled version was run for a GCFR fuel rod analysis. Figure 2-52 shows the film heat transfer coefficients along the fuel column for LIFE-IV and LIFE-III. The slight variation of the results of the two codes is caused by the difference between Eqs. 2-16 and 2-17. This also reflected in the cladding o.d. temperatures (Fig. 2-53).

Although the LIFE-IV master version has been completed, validation (i.e., verification and qualification) of the code is still in progress. Hence, LIFE-IV has not been officially released for design analysis. Once it is finalized, it will replace LIFE-III for GCFR fuel rod analysis.

Fig. 2-52. Fuel rod heat transfer coefficient

Fig. 2-53. Cladding o.d. temperature

2.1.3.2.2.2. Advanced Alloy Evaluation for GCFR Cladding Application

After intensive screening of over 80 alloys, 6 advanced alloys were selected as the most promising cladding/duct candidate materials for the LMFBR: ferritic steel (HT9, D57), modified austenitic 316 stainless steel (D9), and high-nickel alloys (D21, D66, and D69). The performance of fuel rods clad with advanced alloys and rods clad with 20% cold-worked 316 stainless steel was compared for liquid metal fast breeder reactor (LMFBR) fuel rod designs (Ref. 2-65). Methods and algorithms were devised to determine the rank of the advanced alloys. The steady-state performance evaluation was based on the cumulative damage fraction (CDF) and the maximum strain. The relative transient performance capability was evaluated according to the transient usage factor (TUF) for a typical 15% overpower transient and an undercooling transient resulting in a 50% increase of the core coolant axial temperature difference. The overpower TUF was designed to evaluate the ability of the cladding to withstand fuel-cladding mechanical interaction (FCMI) as a result of fuel expansion. The undercooling TUF was formulated as the ratio of the cladding stress resulting from a 50% gas pressure increase to the cladding ultimate strength.

Even though the GCFR fuel rod design is generally similar to that of the LMFBR, there is a significant difference in that the GCFR has a vented fuel rod, and the LMFBR fuel rod is sealed. As a result, the performance characteristics of GCFR and LMFBR fuel rods could be substantially different. For example, the GCFR fuel rod is unlikely to be damaged during an undercooling event because fission gas pressure does not build up. Consequently, the undercooling TUF formulation does not apply to evaluation of vented fuel rod cladding performance. The GCFR cladding structural capability during undercooling transients is likely to be related to deformation effects.

The vented GCFR fuel rod design not only eliminates fission gas pressure buildup, but also reduces the potential for FCMI (Ref. 2-66). However, the GCFR cladding is designed to operate at higher temperatures (2 σ peak midwall temperature of 700°C for the GCFR versus 650°C for the LMFBR),

which reduces cladding strength and changes the swelling and creep characteristics. Owing to these differences, a separate effort to evaluate the advanced alloys for GCFR cladding application was deemed necessary to complement the LMFBR results.

The LMFBR fuel rod design and operating conditions for each candidate alloy were obtained from the COROPT program (Ref. 2-67), optimized for minimum core cost subject to specific design constraints. Because this information is not available for the GCFR, the evaluation was based on the assumption that all fuel rods have the same design and operating conditions given in Table 2-18. The fuel rod geometries were obtained from Ref. 2-68 and the liner power and fast neutron flux from Ref. 2-69 for the 870-MW(t) plant design.

The LIFE-III code was used to evaluate the performance of fuel rods clad with advanced alloys and to provide the initial conditions for evaluating TUF. LIFE-III models the GCFR fuel rod using five axial fuel sections, five cladding rings, six fuel structural rings, and twelve fuel thermal rings. The material properties of cold-worked 316 stainless steel in LIFE-III were unchanged for the clad fuel rod analysis. To analyze the advanced alloy fuel rod, the cladding material properties were updated according to Ref. 2-70. Cladding o.d. wastage was ignored to take advantage of negligible helium corrosion under GCFR fuel rod operating conditions (Ref. 2-71).

Table 2-19 summarizes the steady-state performance evaluation in terms of CDF and total $\Delta D/D$ as calculated by LIFE-III. The CDF values are well below the limiting value of 0.5, which means that the likelihood of an endurance failure resulting from steady-state operation is remote. The highest CDF (0.354×10^{-2}) occurred in the HT9 rod, and the lowest CDF (0.251×10^{-5}) in the cold-worked 316 stainless steel rod. The relatively higher CDF for HT9 is primarily caused by the virtual absence of fluence-induced metal swelling, giving rise to more severe FCMI. The maximum total $\Delta D/D$ always occurred in axial section 3, and only D09, a modified 316 austenitic steel, exceeded the 5.0% $\Delta D/D$ limit.

TABLE 2-18
DESIGN AND OPERATING CONDITIONS

Fuel length (mm)	1130
Cladding inner radius (mm)	3.22
Cladding thickness (mm)	0.416
Pellet radius (mm)	3.15
Pellet density (% theoretical density)	89.4
Coolant inlet temperature (°C)	316
Coolant out temperature (°C)	552.
Peak linear power (W/cm)	386
Peak fast flux n/cm ² -s)	2.85×10^{15}
Peak burnup (at. %)	10.5

TABLE 2-19
EVALUATION OF STEADY-STATE PERFORMANCE (a)

Alloy	CDF			Total Strain ($\Delta D/D$)		
	(CDF) _{max}	(CDF) _r	Location (Axial Section)	($\Delta D/D$) max %	($\Delta D/D$) _r	Location (Axial Section)
316 SS	0.251×10^{-5}	1	5	4.56	1	3
HT 9	0.254×10^{-2}	1012	4	0.938	0.21	3
D09	0.584×10^{-3}	230	4	5.24	1.15	3
D21	0.859×10^{-4}	34	4	2.28	0.50	3
D57	0.740×10^{-3}	295	5	1.00	0.22	3
D66	0.479×10^{-4}	19	4	1.52	0.33	3
D68	0.263×10^{-4}	10	4	1.06	0.23	3

(a) $(CDF)_r = (CDF)_{max} / 0.251 \times 10^{-5}$; $(\Delta D/D)_r = (D/D)_{max} / 4.56$.

Table 2-20 gives the overpower TUF values and factors which influence TUF. The highest overpower TUF occurred in the D21 fuel rod, and the lowest overpower in the cold-worked 316 stainless steel and D09 rods. It is not possible at this time to define a TUF limit above which cladding failure occurs. A relatively high TUF value is merely indicative of relatively poor cladding performance for the given overpower transient. The relatively poor performance of D21 (or, in general, high-nickel alloys) during the overpower transient is primarily caused by its low ductility, even though its ultimate strength is improved. By the same token, the relatively good transient performance of the austenitic steels (316 stainless steel and D09) arises from the combination of moderate strength and ductility. Based on the evaluation the following conclusions have been drawn:

1. The austenitic steels, cold-worked 316 and D09, appear to be the best choice for the GCFR cladding in view of the low CDF and TUF values. However, because of higher swelling, the maximum total $\Delta D/D$ is approximately equal to the limiting value of 5%. Consequently, further clarification of rod bundle-duct mechanical interaction and coolant flow restriction is needed.
2. The ferritic steels (HT9 and D57) are the second choice for GCFR cladding because of their higher ductility, which enables them to withstand FCMI during overpower transients, which appear to be the most damaging events. However, the strength of the ferritic steels at high temperatures (e.g., 750°C) requires further clarification.
3. The high-nickel alloys suffer from poor performance during overpower transients because of their low ductility.

TABLE 2-20
EVALUATION OF OVERPOWER TRANSIENT PERFORMANCE(a)

Alloy	Occurrence Time (h)	$\bar{T}_c$ (°C)(b)	$\bar{T}_f$ (°C)(b)	σ_{eq}^{max} (MPa)(b)	UTS (MPa)	ϵ_u (%)	TUF	(TUF) _r (c)
316 stainless steel	8,000	559	1,059	17.67	548(c)	5.00(c)	0.0405	1
HT9	18,000	598	1,063	18.65	315	10.33	0.0645	1.59
D09	10,000	604	1,084	16.35	488	5.74	0.0402	0.99
D21	11,000	601	1,071	18.84	650	0.11	0.1017	2.51
D57	18,000	598	1,066	18.32	332	4.05	0.0631	1.56
D66	18,000	601	1,059	20.33	762	0.25	0.0850	2.10
D68	18,000	601	1,065	13.03	831	0.25	0.0777	1.92

(a) $\bar{T}_c$ = cladding midwall temperature; $\bar{T}_f$ = mass average fuel temperature; σ_{eq}^{max} = maximum cladding equivalent stress; UTS = ultimate tensile stress; ϵ_u = total engineering of standard tensile specimen; TUF = transient usage factor; TUF_r = relative transient usage factor.

(b) Evaluated at $x/L = 0.7$ except for 316 steel steel, which was evaluated at $x/L = 0.5$.

(c) (TUF)_r = TUF/0.0405.

(d) Unirradiated.

These results are preliminary and require refinement for the following reasons:

1. Although LIFE-III has been validated with respect to experimental data, the properties of cold-worked 316 stainless steel used in the code are not the up-to-date correlations. Therefore, when LIFE-IV becomes available, the analysis must be repeated.
2. Compared with cold-worked 316 stainless steel, most of the advanced alloy property correlations used were based on meager test data developed in Ref. 2-70. The analysis needs to be updated when the updated advanced alloy property correlations become available.
3. The use of $(TUF)_{op}$ to evaluate the cladding overpower transient performance is crude and must be considered an interim, best available method. For example, during FCMI, $(TUF)_{op}$ does not reflect the ability of the cladding to contain the fuel expansion. Therefore, a transient fuel rod performance code with material damage models should be used for further study when it becomes available.
4. Other effects, such as local straining due to fuel cracks, ridging, circumferential temperature variation, and external rod loading, should be taken into account.

2.1.3.2.3. Core Physics Methods Development

Several revisions were made to the DOT-IV code (2-72) because of problems encountered while attempting to use the code with boundary sources. The code currently appears to operate satisfactorily with external boundary

sources for both X-Y and R-theta geometries. However, the internal boundary source option is inoperable. Specific problems include the following:

1. Internal and external boundary source input files do not contain record types 1 through 4. Subroutine SBSRIN was changed for compatibility (I5 and I6 were changed from 5 to 1).
2. An error was discovered in subroutine SBSRIN which affected the writing of boundary sources onto scratch file NDBSI. The logical record length for energy group i was set at $IJNTSR + i * ICMJCM$. This eventually caused the record length to exceed the maximum length permitted. No evidence was found of any need for the additional space, so SBSRIN was changed to write records with a length of IJNTSR.
3. A floating point overflow occurred in subroutine WRFOG when words from array TITL (which contains alphanumeric data) were transferred to array HNAME. This was because array TITL was double precision and array HNAME was single precision. This was corrected by changing PDP element COMIN so that variables TIMAR and TITL were single-precision quantities. All routines which referenced COMIN were then recompiled. A change was also needed in subroutine TIMER to replace the double-precision declaration of variable T with a simple dimension statement.
4. During processing of item 3, a problem arose concerning attempts to recompile subroutine LOCO. The text of the symbolic element on program tapes 4579 and 4697 had to be recreated from the data file contained on file 2 of tape 6461 (written with $\Delta COPY, M$ command).
5. The declaration TYPE SETAFCM was included in the map.
6. Flux output files, which are catalogued files, require a maximum track limit greater than the default of 512 for large problems.

2.2. REACTOR CORE ANALYSIS AND DESIGN

2.2.1. Core Nuclear Analysis

2.2.1.1. Core Physics Analysis. Reference 2-73 gives a preliminary definition of a GCFR demonstration plant. Since that time, the design has undergone a number of major changes as a result of programmatic decisions concerning the purpose of the demonstration plant as well as technological considerations. Most recently a study (Ref. 2-74) to define the characteristics of a first commercial GCFR plant produced a fuel assembly design (Ref. 2-75) which could be employed as the basic element for a demonstration plant core design. Subsequently, attention has been focused on the so-called two-plant commercialization strategy, which might consist of a demonstration plant with full-size prototypical fuel assemblies and a small number (two or three) full-size primary loops. This section reports some of the results of physics nuclear analysis of a two-loop, 400-MW(e) GCFR demonstration plant core and blanket which employ the fuel assembly characteristics attractive for the first commercial size plant.

Figure 2-54 shows the core radial layout, fuel segment distribution, control assembly location, and fuel zones for the 400-MW(e) core. The major differences between this layout and the previous demonstration core design include

1. 400 versus 300 MW(e).
2. 169 versus 127 fuel plus control assemblies.
3. Dedicated (nonfueled) versus partially fueled control assemblies.
4. 2575 versus 1210 kg fissile plutonium.

The fuel management (Ref. 2-76) and rod programming schemes (Ref. 2-77) developed previously were employed to help reduce core power peaking and control system requirements. The outer-to-inner-to-middle row radial blanket management scheme of Ref. 2-78 was used to reduce blanket power peaking and shielding requirements.

The two-dimensional radial depletion using the GAUGE code (Ref. 2-79) was primarily done to provide approximate information on the character and time dependence of radial power distribution. Representative information was used to expedite the study rather than allowing a lack of detailed information to delay definition of any major problems. The time-dependent core model generated will be used in future analysis to provide preliminary information on safety coefficients and shutdown margins.

The 400-MW(e) analytical model was fueled, refueled, and shuffled in the manner expected for the initial core through reload three. Table 2-21 presents the peak linear heat rates observed for the four equivalent full power years of operation. Based on the information available from the depletion and recognizing that fuel loadings, fuel zoning, and rod programming can be improved, a number of tentative conclusions can be drawn:

1. A radial local (fuel rod/core average) power peak of ≤ 1.45 is easily achievable.
2. A (fuel assembly/core average) power peak of ≤ 1.40 is easily achievable .
3. The (fuel rod/fuel assembly) power peak will approach 1.30 for the corner assemblies; the other outer assemblies will approach 1.20.
4. The maximum linear heating rates in the radial blanket are 30% lower than those in the core.
5. Optimum zoning may reduce power peaking from 5% to 10%.
6. The proposed 400-MW(e) design appears to present no insurmountable problems with regard to power distribution, but peaking reduction of nearly 15% will be required if circulating power margins are to be maintained.

TABLE 2-21
PEAK LINEAR HEAT RATES(a)

Cycle	Time (days)	Bank Insertion(b)	Peak Patch	Location Assembly	LPF(c)	APF(d)	Peak (kW/m)
1	0	0.310 (A)	9	5	1.186	2.169	32.5
1	140	0.155 (A)	9	5	1.194	2.124	32.0
1	280	0.045 (A)	9	5	1.199	2.081	31.5
2	280	0.913 (B)	2	4	1.039	2.780	36.5
2	420	0.327 (B)	3	4	1.030	2.542	33.0
2	560	0.0 (B)	2	4	1.022	2.349	30.3
3	560	0.169 (A)	1	3	1.006	2.486	31.5
3	700	0.085 (A)	1	3	1.005	2.435	30.9
3	840	0.0 (A)	1	3	1.005	2.393	30.3
4	840	0.557 (B)	1	2	1.006	2.714	34.4
4	980	0.279 (B)	1	2	1.005	2.541	32.2
4	1120	0.0	1	2	1.005	2.348	31.8

(a)It is assumed that all power is produced in the core and radial blanket (i.e., no power produced in the axial blanket). An axial peaking factor of 1.185 is assumed.

(b)Sequence A includes regions 8, 29, and 43; sequence B, regions 22 and 36.

(c)LPF = local peaking factor (i.e., peak/assembly average).

(d)APF = assembly peaking factor (i.e., assembly power/average assembly power, including radial blanket).

A more precise and thorough analysis of a more optimum design will be initiated once the commercialization strategy and demonstration plant configuration are selected.

2.2.1.2. Shielding Physics Design and Analysis. The purposes of the shielding task are to verify the adequacy of the methods and data for the nuclear design of GCFR shields, to evaluate the effectiveness of various shield configurations, and to provide an interface for mechanical and nuclear shield design activities. This task also coordinates and provides liaison with the analytical and experimental shielding program at ORNL.

During the last quarter, several upper and lower plenum shield configurations compatible with natural circulation were proposed. Additional shielding configurations were proposed during this quarter, and two were selected for further study. Several changes were made to the DOT-IV (Ref. 2-70) discrete ordinates radiation transport code.

2.2.2. Core Thermal-Hydraulic Analysis

2.2.2.1. Use of Hot Spot Factors During Transient Calculations. Two sample depressurization transients were analyzed: one in which the hot spot factors were used during the transient to calculate hot spot temperatures and the other in which the hot spot factors were used for the nominal transient temperatures as a final step in calculation. The results show that the method of applying the hot spot factors affects the heat removal and heat storage rates during the transient. This can result in a difference of up to 100°C in blanket cladding temperature.

Hot spot factors have been defined as the effects of tolerances and uncertainties for all the temperature drops in the core. For example, the uncertainty for the cladding o.d. has a channel factor to account for the flow diverted from the smaller subchannel to the other subchannels. It also has a film factor to account for the change in the film coefficient due to lower flow and higher coolant temperature. Including both factors in the transient is like applying the film factor twice to the film drop.

The use of locally occurring hot spot factors as defined in Refs. 2-80 and 2-81 for calculating fuel temperatures is also incorrect. These factors are defined as effects on cladding midwall temperatures. Effects on the cladding i.d. and the fuel may be much smaller, or there may be none at all owing to circumferential conduction in the cladding.

Figures 2-55 and 2-56 show the exit coolant temperatures for the two rods. Curve C gives the transients for calculating hot spot temperatures, and B gives the hot spot temperatures calculated from nominal transient A. The difference between curves B and C indicates that the method of calculating hot spot temperatures affects the heat removal and heat storage rates of the rods. This difference is large (over 100°C) for the blanket rod.

Figures 2-57 and 2-58 show cladding midwall temperatures. Since the film and cladding ΔT values are small compared with the channel ΔT , the same trend can be seen, with one exception. Curves B and C do not coincide for the fuel at steady state because the heat transfer coefficient is a function of the coolant temperature. The applied hot film factor for curve C is reduced from 15% to 10% owing to improved heat transfer at the higher temperature. In fact, the entire effect of the difference in heat removal rates is due to the temperature dependence of the film coefficient and the cladding conductivity.

In summary, there are basically two types of uncertainties: those which vary with space, affecting an entire rod or subchannel, and those which vary with time and space or occur locally. It is recommended that the uncertainties which vary with space be included in the transient calculations, but not as factors for steady-state calculations, so that the cumulative effect is not lost. The uncertainties should be applied only to power and flow rates. For consistency and easy comparison of the modeling methods, the same is recommended for the steady-state analysis with SCRIMP (Ref. 2-82). Table 2-22 presents the uncertainties to be included in the transient calculations. After the transient calculation, the temporal and

Fig. 2-55. Exit coolant temperature for fuel rod

Fig. 2-56. Exit coolant temperature for blanket rod

Fig. 2-57. Peak fuel rod temperatures

Fig. 2-58. Peak blanket temperatures

TABLE 2-22
3 σ UNCERTAINTIES TO BE INCLUDED IN TRANSIENT CALCULATIONS
(LAMINAR FLOW CONDITIONS)

	Fuel		Blanket	
	Power Uncertainty	Flow Uncertainty	Power Uncertainty	Flow Uncertainty
Statistical uncertainties				
Cladding o.d.		1.035		1.002
Wire wrap o.d.				1.003
Pellet o.d.	1.013		1.002	
Fissile content	1.010			
Orifice tolerance		1.050		1.050
Total	1.016	1.061	1.002	1.050
Cumulative uncertainties				
Thermohydraulic methods	1.020		1.020	
Physics methods	1.040		1.030	
Total	1.061	1.000	1.051	1.000
Total 3 σ uncertainties	1.078	1.061	1.053	1.050
Total 2 σ uncertainties	1.072	1.041	1.052	1.033

local factors should be applied to the resulting temperatures (Tables 2-23 and 2-24). The total effect loses some of the advantage of statistical addition, but it is small and toward the conservative side.

2.2.2.1.1. Fuel Assembly Peak Cladding Temperature During Natural Circulation Flow Conditions

The demonstration plant fuel assembly (Ref. 2-68) has been analyzed to determine the peak transient cladding temperatures which can be expected during a protected loss of flow transient. Previous transient analyses indicated that the peak temperatures occurred 100 s into the transient. Using the boundary conditions from the transient model, the steady-state SCRIMP thermal-hydraulic code was used to analyze the design in greater detail and, in particular, evaluate the effect of cladding conduction and radiation heat transfer on maximum temperature.

A strip from corner to corner of the fuel assembly duct was modeled (Fig. 2-59). The model included internal, wall, and corner fuel rods and a portion of the duct wall and consisted of five axial sections for hydraulic calculations. Each section was divided into 21 nodes for rod temperature calculations. Hot spot factors at a 2 σ confidence level were used to calculate peak temperatures. With the conservative assumption that interchannel flow mixing is negligible under these low flow conditions, the SCRIMP code indicates that the corner rod temperature will peak at 902°C, approximately 200°C higher than the peak temperatures on the internal rods. Figure 2-60 compares the cladding axial temperature profiles of the corner (A) and interior (B) fuel rods.

2.2.2.1.2. Edge Spacing Analysis.

COBRA runs were performed using a one-twelfth section of a 271-rod GCFR fuel assembly. The following design features were used: rod diameter = 8.0 mm; rod pitch = 11.1 mm; edge spacing = 80%; roughening of the heated length = 100%, heated length = 1200 mm; internal support rods = 6; spacer

TABLE 2-23
HOT SPOT FACTORS FOR FUEL ROD TEMPERATURES
(LAMINAR FLOW CONDITIONS)

	Uncertainty 3σ	Channel Factor F_c	Film Factor F_f	Cladding Factor F_{cl}	Gap Factor F_g	Fuel Factor F_{fuel}
Statistical uncertainties						
Cladding o.d. thickness	0.27%			1.051		
Cladding i.d. thickness	0.31%			1.051		
Film coefficient	10%		1.100			
Friction factor	10%	1.059				
Cladding conductivity	10%			1.100		
Gap conductivity	48%				1.480	
Fuel conductivity	10%					1.100
Total		1.059	1.100	1.112	1.480	1.100
Cumulative uncertainties						
Circumferential heat transfer variation	10%		1.020 (1.000)(a)			
Physics methods			1.010	1.010	1.010	1.010
Inlet flow maldistribution	0.5%	1.005				
Inlet temperature	6°C	1.037(b)				
Total		1.042	1.030 (1.010)(a)	1.010	1.010	1.010
Total hot spot factors						
3σ		1.103	1.133 (1.111)(a)	1.123	1.495	1.111
2σ		1.083	1.099 (1.077)(a)	1.086	1.333	1.081

(a) Values in parentheses are used for fuel temperature calculations.

(b) In addition to the channel effect, the temperature of all the core components is raised 6°C.

TABLE 2-24
HOT SPOT FACTORS FOR FUEL ROD TEMPERATURES
(LAMINAR FLOW CONDITIONS)

	Uncertainty 3 σ	Channel Factor F _c	Film Factor F _f	Cladding Factor F _{cl}	Gap Factor F _g	Fuel Factor F _{fuel}
Statistical uncertainties						
Cladding o.d. thickness	0.06%			1.027		
Cladding i.d. thickness	0.06%			1.027		
Heat transfer coefficient	12%		1.120			
Friction factor	42%	1.233	1.15465			
Cladding conductivity	10%			1.100		
Gap conductivity	48%				1.480	
Fuel conductivity	10%					1.100
Total		1.233	1.197	1.109	1.480	1.100
Cumulative uncertainties						
Circumferential heat transfer variation	25%		1.100 (1.000)(a)			
Physics methods			1.050	1.050	1.050	1.050
Inlet flow maldistribution	0.5%	1.000	1.003			
Inlet temperature	6°C	1.024(b)	1.013			
Total		1.029	1.174 (1.067)(a)	1.050	1.050	1.050
Total hot spot factors						
3 σ		1.269	1.405 (1.277)(a)	1.159	1.554	1.155
2 σ		1.189	1.328 (1.207)(a)	1.123	1.386	1.120

(a) Values in parentheses are used for fuel temperature calculations.

(b) In addition to the channel and film effects, the temperature of all the core components is raised 6°C.

Fig. 2-59. Strip model of fuel assembly

Fig. 2-60. Hot spot temperatures for corner and internal rods at 100 s

configuration = true hex. When the same spacer design and material thicknesses as the CFTL AG-1 design were used, the maximum edge rod diametral temperature difference was 57°C. Additional runs were performed with edge spacings of 75% and 65% and with different edge channel blockages in an attempt to reduce the rod temperature difference. For all edge clearances, it was found that the maximum positive rod diametral temperature difference (inner minus edge channel) remained essentially constant, but the temperature swings due to spacers decreased as the edge blockage decreased. Furthermore, the maximum positive rod diametral temperature differences along the heated length coincided with the differences in bulk helium temperature between the inner and edge channel. The rod temperature difference length plots resulting from this work will be subjected to a rod stress and bowing analysis. Additional design variations will be explored with the objective of reducing the rod diametral temperature difference on an edge rod.

2.2.2.2. Radial Blanket Assembly.

2.2.2.2.1. Radial Blanket Peak Cladding Temperature During Natural Circulation Flow Conditions

Natural circulation under protected loss of flow accident conditions was previously analyzed by a transient system code for a 300-MW(e) GCFR. The results showed that the peak cladding temperature of 1149°C in the maximum-powered radial blanket assembly would occur ~100 s after shutdown. Since the transient analysis modeled the entire primary system and the CACS, the details of modeling the reactor portion were compromised. Hence, a study was performed to examine the effects of the missing details. By treating the time corresponding to peak cladding temperature as a quasi-steady-state condition, this analysis explored the thermal-hydraulic effects of the blanket assembly using the steady-state rod bundle code SCRIMP, which considers all three heat transfer mechanisms: conduction, convection, and radiation.

The maximum-powered blanket assemblies are located at the center of a hexagonal flat of blanket assemblies adjacent to the active core. It would be very costly to carry out rod bundle calculations for an entire assembly. Since the power distribution in the blanket assembly is exponentially reduced in a radial direction away from the core, a strip model with adiabatic boundary conditions (conservative) along the center of the assembly was employed (Fig. 2-61). The following assumptions were used in this analysis:

1. Quasi-steady state.
2. No turbulent mixing between flow channels (conservative).
3. End of equilibrium cycle (EOEC) reactor condition (maximum power).

Since the transient peak cladding temperature occurs only 100 s after shutdown, the heat storage effect in the transient analysis is not pronounced. Therefore, it is sufficient to employ exact decay power for the steady-state analysis. According to Ref. 2-80, the latest decay heat algorithm is

$$P(t) = C_1(P_0 + G_1)e^{-t/18} + C_2(t)(P_0 + G_2/0.07 t^{-d} + P_0 C_e^{-t/(2.924 \times 10^5)} + P_0 C_4 e^{-t/2034} \quad , \quad (2-18)$$

where P_0 = preshutdown linear power,

C_1 = decay heat linear power at time zero as a fraction of preshutdown linear power (except for C_2 , which is time dependent),

G_1 = preshutdown gamma energy transported from the core to the radial blanket,

t = time.

The four contributing terms in Eq. 2-18 are

1. Fission energy after shutdown due to delayed neutrons.
2. Energy from the decay of fission products.
3. Energy from the decay of the Np-239 breeding product.
4. Energy from the decay of the U-239 breeding product.

Fig. 2-61. Strip model of maximum-powered blanket subassembly (not drawn to scale)

Gamma heating in the duct wall is also accounted for because the peak-powered rod is the corner rod next to the duct wall adjacent to the core. Therefore, this decay heat calculation also provides the radial power distribution of the rod bundle. According to Ref. 2-81, the hot spot factors employed for low-flow conditions are

$$\begin{aligned}f_{\text{channel}} &= 1.194, \\f_{\text{film}} &= 1.272, \\f_{\text{cladding}} &= 1.249.\end{aligned}$$

The relevant results at the elevation of the cladding hot spot temperature are tabulated below.

<u>Cladding Surface</u>		<u>Coolant Channel</u>	
<u>Surface No.</u>	<u>Temperature (°C)</u>	<u>Channel No.</u>	<u>Temperature (°C)</u>
1	742	1	680
2	726	2	624
3	746 (nominal maximum)	3	678
4	718	4	652
5	706		
6	690		

With the hot spot factor applied, the peak cladding surface temperature is 854°C, which is considerably lower than the previous result of 1149°C obtained by the transient calculation. The transient calculation modeled only the peak-powered rod (rod 56) with a nominal rod unit cell flow channel and ignored the existence of the large edge channel (channel 2) which provided better cooling. In addition, the transient modeling could not take account of the effective circumferential conduction and radiation effects of the rod bundle. Based on the results of this study, it is strongly recommended that the transient code should model the second-row rod (rod 57), which is the second highest-powered rod, to compensate for the overly conservative method of the code.

2.2.2.2.2. Effect of Wire Wrap Spacer System on Assembly Pressure Drop

The addition of a wire-wrap spacer system to a bare rod bundle causes a significant increase in the pressure drop under turbulent flow conditions. Two different effects must be taken into account: the increase in friction factor and the decrease in hydraulic diameter. Table 2-25 shows the effect of the wire on the bundle friction factor. The data for the wire-wrapped bundles were calculated from the correlation developed in Ref. 2-83; the data for the bare rod bundles were taken from Fig. 114 of Ref. 2-84. The ratio of the friction factors was calculated for two different values of pitch-to-diameter ratio (p/d) as a function of Reynolds number (Re). Although the effect of Re is small, increasing p/d from 1.02 to 1.10 causes the effect on friction factor to drop from an average of 1.63 to 1.26.

Table 2-26 shows the calculation of the pressure drop ratio. Addition of the wires caused changes in the subchannel flow area and the wetted perimeter. With one wire per rod at any axial location, each interior subchannel contained an average of one-half of one wire. When the assumption of equal Re was made for the two configurations, the pressure drop ratio was linear in the friction factor ratio, but a cubic function of the hydraulic diameter ratio. For a small p/d , the hydraulic diameter ratio effect was small. However, for a p/d of 1.10, the effect was large, more than compensating for the friction factor ratio reduction. The effect of varying rod diameter on these results is small.

It can be concluded from this analysis that, unlike the effects experienced in grid-spaced rod bundles, an increase in rod p/d in a wire-wrapped rod bundle results in an increase in the relative effect of the wires on the pressure drop through the bundle. For the range of parameters investigated, the wire-wrap spacer system increased the pressure drop through the bundle by approximately 80%.

TABLE 2-25
FRICTION FACTOR RATIO^(a)

Novendstern equation (Ref. 2-81)

$$f_N = \left[\frac{1.034}{(p/d)^{0.124}} + \frac{29.7(p/d)^{6.94} Re^{0.086}}{(h/d)^{2.239}} \right]^{0.885} \left\{ 2 \log \left[\frac{-5.028}{Re} \log \left(\frac{16.76}{Re} \right) \right] \right\}^{-2}$$

Bare rod bundle (Ref. 2-82)

<u>p/d</u>	<u>Re</u>	<u>f_N</u>	<u>f_B</u>	<u>f_N/f_B</u>
1.02	30 K	0.0271	0.0168	1.61
	50 K	0.0243	0.0152	1.60
	70 K	0.0227	0.0136	1.67
	100 K	0.0211	0.0128	1.65
1.10	30 K	0.0290	0.0240	1.21
	50 K	0.0260	0.0205	1.27
	70 K	0.0243	0.0195	1.25
	100 K	0.0227	0.0172	1.32

-
- (a) p = rod pitch,
d = rod diameter,
h = wire pitch,
N = wire-wrapped,
B = without wire wrap.

TABLE 2-26
PRESSURE DROP RATIO(a)

$$\frac{\Delta P_N}{\Delta P_B} = \frac{f_N L/D_N 1/2 \rho V_N^2}{f_B L/D_B 1/2 \rho V_B^2} = \frac{f_N V_N^2 D_B}{f_B V_B^2 D_N}$$

Assume

$$Re_N = Re_B ,$$

$$\frac{\rho V_N D_N}{\mu} \left(\frac{\mu}{\rho V_B D_B} \right) = 1 ,$$

$$\frac{V_N D_N}{V_B D_B} = 1 ,$$

$$\frac{V_N^2}{V_B^2} = \frac{D_B^2}{D_N^2} ,$$

$$\frac{\Delta P_N}{\Delta P_B} = \frac{f_N}{f_B} \left/ \frac{D_N^3}{D_B^3} \right. ,$$

$$D = \frac{4A}{P_w} , A = \frac{\sqrt{3}}{4} P^2 - \frac{\pi}{8} (d_r^2 + d_w^2) ,$$

$$P_w = \frac{\pi}{8} (d_r + d_w) ,$$

$$\frac{D_N^3}{D_B^3} = \left(\frac{d_r}{d_r + d_w} \right)^3 \left[\frac{2\sqrt{3}P^2 - \pi(d_r^2 + d_w^2)}{2\sqrt{3}P^2 - \pi d_r^2} \right]^3$$

Therefore, for $d_r = 15 \text{ mm}$, $d_w = p - d_r$,

$$\frac{\Delta P_N}{\Delta P_B} = \frac{f_N/f_B}{\left(\frac{d_r}{d_r + d_w} \right)^3 \left[\frac{2\sqrt{3}P^2 - \pi(d_r^2 + d_w^2)}{2\sqrt{3}P^2 - \pi d_r^2} \right]^3}$$

TABLE 2-26 (continued)

<u>p/d</u>	<u>Re</u>	<u>f_N/f_B</u>	<u>ΔP_N/ΔP_B</u>	
1.02	30 K	1.61	1.73	} Average = 1.76
	70 K	1.67	1.79	
	100 K	1.65	1.77	
1.10	30 K	1.21	1.76	} Average = 1.83
	70 K	1.25	1.82	
	100K	1.32	1.92	

(a) p = pressure,
 D = hydraulic diameter,
 A = flow area,
 P_w = wetted perimeter,
 V = velocity,
 r = rod,
 w = wire.

2.2.2.3. Control Assembly. A preliminary design of the dedicated control rod for a 400-MW(e) GCFR demonstration reactor has been established based on the trade-offs of physics, thermal-hydraulics, and B_4C pellet swelling calculations. Because the GCFR control rod has a vented design, internal gas pressure buildup is not of concern. This study used a 61-rod bundle geometry with 55 control rods and 6 hanger rods. The B_4C pellet contained less than 30 at. % B-10 and offered an in-core residence time of more than 2 yr. Table 2-27 and Fig. 2-62 give the relevant design parameters.

2.2.2.4. Radial Shield Assembly. A preliminary thermal analysis of a representative radial reflector/shield assembly was performed to size the required coolant mass flow rate and to determine the resulting lateral temperature gradients across the rods. The shield assembly was assumed to have the following characteristics:

No. of rods	19
Rod o.d.	38 mm
Cladding thickness	1.5 mm
Rod pitch/diameter	1.03
Spacer type	Wire wrap
Wire diameter	1.165 mm
Wire pitch	279 mm

The analysis was carried out for EOL conditions, when the assembly produces the maximum power. At the middle of life, a maximum of 2% radial shrinkage of the graphite pellet is expected. For this analysis, the maximum gap was assumed at EOL. A flow rate of ~96 kg/h was determined to be sufficient to cool the graphite and the cladding to 700°C. The temperature rise of the coolant across the assembly was about 60% of the core ΔT , but since the total shielding flow rate was ~0.10% of the core flow, the outlet temperature deterioration was small.

TABLE 2-27
PRELIMINARY DESIGN OF A 61-ROD DEDICATED CONTROL
ROD ASSEMBLY

Number of B ₄ C rods	55
Number of hanger rods	6
Cladding o.d.	16.8 mm
Cladding i.d.	15.8 mm
Rod-to-rod gap	4.1 mm
Lattice pitch	20.9 mm
Edge spacing (~80%)	3.2 mm
B ₄ C pellet-cladding gap	To be determined
Guide tube duct	
Across flats o.d.	176 mm
Across flats i.d.	168 mm
Subassembly duct	
Across flats o.d.	194 mm
Across flats i.d.	186 mm

Fig. 2-62. Preliminary design of 61-pin dedicated control rod.

Figure 2-63 shows the maximum graphite temperatures, and Fig. 2-64 presents the maximum temperature drops across the cladding, with no circumferential conduction considered. Because the heat transfer coefficient was small (the flow regime is laminar), circumferential conduction was dominant. Inclusion of circumferential conduction resulted in an order of magnitude decrease in the lateral temperature drop (to 11°C).

There were problems at full power as far as maximum temperatures and gradients are concerned. At low powers, however, the hydraulic resistance of the shielding assemblies will change drastically. Since the resistance of the fuel assemblies does not change much, providing coolant at the required proportions to different core assemblies at low powers will be a problem. A possible solution is to overcool these assemblies at full power. Increasing the flow to 10 times the required amount makes the coolant flow turbulent, and the core outlet temperature deterioration is less than 10°C. With this increase in flow, a control device can be installed so that the resistance of the shielding assembly changes in the same proportion as that of the rest of the core, from full to low flow.

2.2.2.5. GCFR Hot Spot Factors for Steady-State Calculations. Film factors for the fuel have been revised according to new data on circumferential heat transfer variation in rough bundles. The values to be used for centerline temperatures were established, and it was determined that an inlet temperature uncertainty of 6°C should be added to the final temperatures in addition to the hot spot factors. At present, the sets of value for pressurized and depressurized laminar flow conditions are the same.

2.2.3. Core Configuration and Performance Analysis

The basis for a six-loop 3600-MW(t) commercial plant and for a corresponding two-loop demonstration plant has been reviewed. After studies showed that higher PCRV pressures were feasible, the pressure was increased from 10 to 10.5 MPa for component design and 11 MPa for PCRV capability to provide a design margin. The core length-to-diameter ratio was set at about 0.25 after a detailed comparison with a ratio of 0.30 showed very

Fig. 2-63. Maximum graphite centerline temperatures ($^{\circ}\text{C}$) (not all occur at same axial location)

Fig. 2-64. Maximum temperature drops ($^{\circ}\text{C}$) across cladding without circumferential conduction (not all occur at same axial location)

little economic difference between the two values. The shorter core was preferable for development and testing in available fast reactors and had a somewhat shorter doubling time.

2.2.4. Core Structural Analysis

2.2.4.1. Advanced Alloy Bowing Study. Six alloys were selected as candidate reference materials for future cladding and duct applications: HT9, D9, D21, D57, D66, and D68. Final selection of an advanced alloy for use in the GCFR will be based on evaluation of material properties which affect the thermal-hydraulic, nuclear, and structural behavior of the fuel assembly. During the previous quarter, a comparative study of the fuel rod bowing of these advanced alloys was performed from the standpoint of structural response to assess their potential for use in GCFR fuel rod cladding. The mechanical layout and temperature distribution of recently completed CFTL bundle AG-1 was employed. (AG-1 is representative of the most recent GCFR fuel assembly designs.) Table 2-28 summarizes the major fuel rod mechanical data. Figure 2-32 shows the spacer grid layout, and Fig. 2-65 presents the cladding temperatures and temperature gradients representative of a GCFR edge rod.

The rod bowing studies were performed using the CRASIB code (Ref. 2-15). The rod was modeled as a variable cross section beam, and the spacers were considered simple supports. The goal burnup of the rod was 100 MWd/kg, which corresponds to a lifetime of 22,000 h and a peak neutron fluence of $2.2 \times 10^{23} \text{ n/cm}^2$. Figures 2-66 through 2-70 present the results of these analyses. Figure 2-66 shows the deflected rod shape at 12,000 h for rods of 316 stainless steel and D9. As expected, the deflected rod shapes were similar. Rod bowing for the D9 rods was considerably less than that for the 316 stainless steel rods. Even the D9 rod bowed excessively, however. For typical GCFR fuel assembly geometries, the maximum permissible bow is about 1 mm (or 55 MWd/kg). This limit was reached with D9 by 12,000 h. With some arrangement of grids and/or reduction in temperature gradients the lifetime of D9 rods can be extended to perhaps 70 MWd/Kg, but it is unlikely that 100 MWd/kg could be achieved.

TABLE 2-28
ADVANCED ALLOY BOWING STUDY
FUEL ROD MECHANICAL DATA

Rod o.d. in smooth regions (mm)	8.0
Rod root o.d. in ribbed regions (mm)	7.74
Rod i.d. (mm)	6.98
Rod length (mm)	2860
Heated length (mm)	1200
Peak fast flux ($n/cm^2-s \times 10^{15}$)	2.83

Fig. 2-65. T and ΔT values for advanced alloy bowing study

Fig. 2-66. Rod bowing of 316 stainless steel and D9 at 12,000 h

Fig. 2-67. Rod bowing of D21 at EOL

Fig. 2-68. Rod bowing of D66 and D68 at EOL

Fig. 2-69. Rod bowing of HT9 and D57 at EOL

Fig. 2-70. Rod bowing of HT9 and D57 at EOL

The performance of the other alloys was considerably better. Figure 2-67 shows the bowing of the D21 rods at 100 MWd/kg (note the scale change from the previous figure). The maximum bowing for this case was about 1.2 mm. With some design modifications, burnups of 100 MWd/kg seemed achievable with D21 cladding.

Figure 2-68 shows the performance of D66 and D68 cladding. The bowing of these alloys is even better, with the peak bowing being less than 0.4 mm. From a rod bowing standpoint, D66 and D68 rods could easily achieve the target burnup of 100 MWd/kg. By far the least bowing was obtained with HT9 and D57 cladding. Figure 2-69 shows the bowing for these alloys; Fig. 2-70 gives an expanded view. The peak rod deflections are about 0.1 mm at 100 MWd/kg, only slightly greater than the beginning of life (BOL) thermal bowing. (In actuality, the deflections will be somewhat greater than this because the clearance allowed at the spacer grids, which is on the order of 0.1 mm, was neglected in this analysis.) Such small bowing is benign and poses no lifetime limitations for the rods.

Table 2-29 summarizes the other data generated in the study, including rod-spacer interaction forces and cladding bending stresses. The loads and stresses were generally acceptable for all the alloys studied except for D21, which seemed to have rod-spacer interaction forces and stresses at EOL which were slightly high. These results should be viewed with caution, because it appears that the CRASIB code had some difficulty handling the D21 material properties and may have exhibited some slight instability during the D21 calculations. The rod deflection calculations were not affected by this instability. The higher loads and stresses obtained with D9 compared with 316 stainless steel are probably due to the better creep resistance of D9. The D9 loads and stresses are still within the accepted limits.

It appears that alloys HT9 and D57 offer outstanding rod bowing performance. Rods clad with either of these alloys would easily achieve burnups of 100 MWd/kg or more. Alloys D66 and D68 would also be able to

TABLE 2-29
RESULTS OF ADVANCED ALLOY BOWING STUDY(a)

Material	Time	δ (mm)	Θ (deg)	P (N)	σ (MPa)	$\Delta L/L$ (%)
316 stainless steel	0	0.11	0.09	2.6	36.0	0
	10,000	2.3	2.0	3.8	22.3	2.7
	20,000	10.0	8.6	2.3	22.0	11.5
D9	0	0.10	0.09	2.4	33.0	0
	12,000	1.3	1.0	6.0	35.7	1.9
	22,000	5.4	4.6	6.0	48.2	8.1
HT9	0	0.08	0.06	1.7	23.9	0
	22,000	0.12	0.09	1.8	35.1	0.2
D21	0	0.11	0.09	2.5	34.9	0
	22,000	1.2	0.98	11.9	87.9	1.7
D57	0	0.08	0.06	1.7	23.9	0
	22,000	0.08	0.08	1.4	6.9	0.1
D66	0	0.10	0.08	2.1	29.8	0
	22,000	0.32	0.27	2.6	15.5	2.6
D68	0	0.09	0.08	2.2	31.4	0
	22,000	0.31	0.25	2.8	26.2	0.5

(a) = maximum deflection; Θ = maximum rotation at a spacer grid; P = maximum rod-spacer interaction force; σ = maximum bending stress (MPa); $\Delta L/L$ = maximum linear swelling.

achieve burnups of 100 MWd/kg or more. Alloy D21 may be able to achieve burnups of 100 MWd/kg, although some design changes would be needed.

Alloy D9 offers very distinct improvements in rod lifetime over 316 stainless steel, although achievement of the goal burnup of 100 MWd/kg with D9 cladding appears unlikely. At the very best, with careful design and attention to the causes of rod bowing, burnups of 70 MWd/kg might be obtained with D9 cladding.

2.2.4.2. Duct Dilation of Advanced Alloys. The six alloys were also evaluated with respect to duct wall dilation. Table 2-30 summarizes the pertinent data,. Wall thickness was varied to provide a basis for design decisions in the alloy selection process. Figure 2-71 presents the results of the evaluation. For the current wall thickness (4 mm), HT-9, D57, and D68 are superior to the other alloys owing mostly to their small swelling. However, at smaller wall thicknesses, the relative difference in creep becomes dominant, and D66 seems to be superior to HT-9. D57 and D68 follow in order of increasing performance.

There are two other trade-offs which must be considered in choosing an alloy based on duct dilation performance. A thin wall and a less superior alloy may be accommodated if the interassembly gap is widened. This may degrade the nuclear and thermal-hydraulic performance. In addition, allowing large amounts of dilation substantially increases edge channel rod thermal gradients and associated rod bowing. The second trade-off is to increase the wall thickness, possibly with a less superior alloy, so that very little dilation occurs. This allows close spacing of the assemblies and close rod bundle duct wall clearances. An optimization study is necessary to choose the best alloy from a combined nuclear, thermal-hydraulic, and structural standpoint.

TABLE 2-30
HOT SPOT FACTORS FOR GCFR FUEL AND BLANKET TEMPERATURES

	Channel F_c	Film(a) F_f	Cladding F_{cl}	Gap F_g	Fuel F_{fuel}
Fuel					
Turbulent flow					
3σ	1.131	1.361 (1.215)	1.195	1.585	1.178
2σ	1.111	1.311 (1.170)	1.153	1.414	1.142
Laminar flow					
3σ	1.236	1.212 (1.188)	1.138	1.585	1.178
2σ	1.187	1.173 (1.150)	1.116	1.414	1.142
Blanket					
Turbulent flow					
3σ	1.196	1.788 (1.277)	1.252	1.630	1.212
2σ	1.151	1.710 (1.222)	1.201	1.454	1.175
Laminar flow					
3σ	1.332	1.477 (1.342)	1.142	1.630	1.212
2σ	1.249	1.395 (1.268)	1.128	1.454	1.175

(a) Values in parentheses were used for centerline temperatures.

Fig. 2-71. Duct dilation vs wall thickness for various alloys

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3.0. PROJECT SUPPORT

3.1. QUALITY ASSURANCE

There was no activity on this task during this quarter.

3.2. PLANT SIMULATION

3.2.1. On-Load Plant Control Development

On-load plant control analysis has continued for less than full-load conditions, ramp load changes, and step transients. The control system gain configuration for well behaved transient responses at full load produced excessive main stream temperature and pressure limit cycle oscillations and control rod jogging at the 50% power level. Control system gain changes were required to restore the desired system response, indicating that a gain change schedule will be needed to provide the desired system responses over the full load range. Main steam temperature and pressure are closely coupled in this system.

Figure 3-1 shows the simulation results for a 5%/min ramp load change from full to three-quarters load. The full-load control system gain configuration was used, and the system responses were reasonably well behaved. For this simulation, circulator speed was only an open-loop function (using a characterizer) of feedwater flow (Fig. 3-2). The averaged module steam exit temperature trim control loop around the circulator speed controller could not be incorporated into this single-loop plant model.

3.2.2. Simulation Method Development

The single-loop plant model was expanded to include a second independently modeled helium loop and feedwater/steam loop. This two-loop model will enable simulation of loop interactions in multiloop plants and

Fig. 3-1. Ramp load change from full to three-quarters load (sheet 1 of 4)

Fig. 3-1. Ramp load change from full to three-quarters load (sheet 2 of 4)

Fig. 3-1. Ramp load change from full to three-quarters load (sheet 3 of 4)

Fig. 3-1. Ramp load change from full to three-quarters load (sheet 4 of 4)

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Fig. 3-2. Plant control configuration

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development of plant control loop trimming functions. A once-through steam generator model was developed which has the capability to simulate input/output process dynamics during the normal steaming operation, the transition phases of flood-out (from normal steaming to all water-flow) and startup, and all-water operation. This steam generator model is being incorporated into the single and multiloop plant models.

3.3. SYSTEMS ENGINEERING

3.3.1. Systems Integration

Licensing criteria for GCFR core disruptive accidents (CDAs) which set the ground rules for the analysis/evaluation of CDAs were established. One of the points stipulated in the criteria is that containment integrity shall be maintained (1) as long as all systems designed to mitigate CDAs perform as designed and (2) for a "to be determined" period of time in the event of a failure of any of the mitigating systems.

A new GCFR plant specification on reliability requirements which requires that reliability analyses be integrated into all phases of GCFR safety system design was authorized. Licensing criteria were provided for shutdown systems, and work was initiated on developing information for potential use in comparing the safety of GCFRs, LWRs, and LMFBRs, with particular emphasis on thermal capacity.

The RECS implementation program continued. The activities on the various system networks were reviewed, and modifications to the networks were incorporated. The initial round of level 2 system-to-system interface identification was completed.

3.3.2. System Design (NSSS)

3.3.2.1. Upflow/Downflow Studies. The upflow/downflow study has been completed (Ref. 3-1).

3.3.2.2. Residual Heat Removal. The first set of consistent transient and steady-state CACS analyses was completed. This analysis included the following operating conditions:

1. Natural circulation pressurized cooldown.
2. Forced circulation pressurized cooldown.
3. Forced circulation DBDA (0.0484-m² leak).

The data will be used for sizing and analysis of the CAHE.

A study of a boiling CACS was initiated, and interim results indicate that it is possible to design a natural circulation, recirculating boiler-type CACS which will not require water pumps or air fans. However, because of the large heat duty variation between standby operation on cold days and emergency operation on hot days, maintaining the water temperature and pressure swing at reasonable levels requires the use of louvers to control air flow through the auxiliary loop cooler (ALC). Evaluation of component sizes for this system resulted in heat exchangers which are nearly the same size as those in the all-liquid system, except for some minor modifications. These components are compatible in both the six- and two-loop designs.

The steam generator's capability for "self-isolation" during pressurized natural convection cooldown of the CACS was evaluated. (Self-isolation is the inherent ability to suppress bypass flow, using buoyancy forces, even with the main loop helium isolation valves open.) The self-isolation capability is design dependent. The bottom exhaust steam generator probably cannot be easily designed for self-isolation, but it is likely that the top exhaust steam generator can. However, behavior during cooldown transients must be investigated to determine the effect of an initially hot steam generator on self-isolation characteristics. The current study was based on only quasi-steady-state conditions.

Natural convection into the top CAHE cross ducts during normal reactor operation was investigated, assuming that the CACS cross ducts would be directly coupled to the core cavity by simple, straight helium ducts. Preliminary calculations indicate that hot helium from the core plenum will circulate into the top CAHE cross ducts during normal reactor operation and mix with cold flow leaking by a closed CACS isolation valve and through the CAHE loop in the reverse direction. This conclusion is based on data for suppression of natural convection in vertical and horizontal ducts which show that the CAHE loop reverse flow is not sufficient to prevent hot upper plenum helium from entering the top CAHE cross ducts.

Preliminary screening of alternate CACS fluids was completed. The objective of the screening was to evaluate means of eliminating freezing and boiling problems. Although no ideal fluid was found, a eutectic sodium-potassium (NaK) liquid metal alloy had some potential. Strict criteria for material compatibility (including PCRV leaks), toxicity, flammability, etc., quickly eliminated most candidate fluids. A more detailed examination of three alternatives (NaK, Dowtherm J, and a 10% ammonia solution) indicated that these fluids were inferior to water.

Preliminary performance data on the demonstration plant integral shutdown cooling system were established. The reactor pressure required for adequate RHR on one loop was determined. It varies from ~689 kPa within the first few minutes after reactor shutdown to ~275 kPa at 3 h and atmospheric pressure after 3 days. The long-term (up to 30 days) depressurized RHR helium conditions were also determined, and estimated performance data (pressurized and depressurized 24 h after shutdown) were calculated.

3.3.2.3. Steady-State Performance and Primary Coolant System. Pressure drops within the primary coolant loop were determined for both the six-loop commercial and two-loop demonstration plants. Various shielding arrangements around the core were evaluated for their effect on helium pressure drop. Although the details of the primary coolant flow geometry in the

major plena and cross ducts were not defined, several possible configurations were analyzed. Thus, a preliminary specification for component pressure losses could be established.

Potential areas of velocity maldistribution and hot gas temperature streaks were identified. These parameters are important for both steam generator and thermal barrier design. It was concluded that a flow model test should be planned to determine the flow distribution in the core outlet plenum, the hot cross duct, and the steam generator gas inlet. Although similar tests have been performed for the HTGR, the unique flow configuration of the GCFR requires a separate test. Potential test requirements were explored, including the flow model, instrumentation, schedule, and cost. It is believed that the GA air flow test facility can be used. The model is tentatively one-third scale and will achieve one-third of the prototypical Reynolds number at design point conditions.

The SCAP code, a steam cycle analysis program originally written for an HTGR, was modified to reflect certain unique GCFR characteristics. The steady-state heat balances now include the GCFR feedwater and turbine systems. The NSSPERFORM code, a primary coolant system analysis code, has been developed and evaluated. It calculates primary side performance and includes multiloop capabilities. A significant amount of additional work is required to complete this code and check it out. Helium system and steam cycle performance data are being prepared to guide plant conceptual design.

3.3.2.4. Parametric Design Models. Parametric studies of the major plant design variables for the large 3600-MW(t) GCFR were performed. Seven variables were studied in terms of their influence on total power cost:

1. PCRV pressure.
2. Core ΔP .
3. Maximum cladding temperature.
4. Core inlet temperature.

5. Core outlet temperature.
6. Main steam temperature.
7. Feedwater temperature.

Other variables were not considered at this time because the cost impact would not have been adequately represented in the present version of the SOFAST code.

The main results of these studies are as follows:

1. There is a cost incentive to increase main steam temperature to the maximum value possible without changing the tube material in the superheater section. The addition of a bimetallic weld in the superheater introduces a step increase in plant cost of approximately \$4.2 M*. A main steam temperature of 789 K with a core outlet gas temperature of about 835 K is near an optimum.**
2. The maximum cladding temperature was assumed to be limited to 750°C. If it were possible to increase this limit, a cost reduction of \$2.3 M per °C could be achieved in total capital cost.
3. The cost saving with the new parameters (compared with the demonstration plant design parameters of January 1979) was approximately \$120/kW(e).

*All costs are quoted in 1979 direct dollars. Fuel costs are expressed as equivalent capital costs. For reference, total plant direct costs are estimated to be approximately \$1100 M.

**The terms "optimum" and "optimum conditions" should be used cautiously. The SOFAST code is still not complete, not all cost effects are modeled, and few are modeled as accurately as they will be.

4. The apparent optimum combination of the design variables studied is as follows:

PCRV pressure	11.13 MPa
Core ΔP	0.18 MPa
Core outlet temperature	835 K
Core inlet temperature	589 K
Feedwater temperature	472 K
Main steam temperature	789 K
Maximum cladding temperature	1023 K*

5. Total power cost does not vary strongly in the vicinity of the apparent optimum. Therefore, final selection of a particular design variable may be governed by factors other than cost alone.

During the course of investigating the major plant design parameters, the results were reviewed and refined, and several improvements were made to the SOFAST code:

1. A correction function was added to the calculation of the PCRV overall dimensions.
2. The effect of feedwater temperature on steam cycle efficiency was redefined in response to recent steam cycle analyses of the 3600-MW(t) plant. This lowered the calculated optimum feedwater temperature from approximately 505 to 477 K.
3. The steam generator model was modified to extend the material property data for chrome-moly steel from 839 to 922 K. Although it is possible to use chrome-moly steam at temperatures up to 922 K, it is desired to hold to the 839 K temperature limit.

*Material limit.

4. Work continued on adding a detailed steel system model into the code.

The plant capital cost relationships used in SOFAST were briefly reviewed and are being modified by including information from CRAM (Ref. 3-2). SOFAST currently predicts higher capital costs than other calculations. Fuel cycle costs were reviewed, and a new expected fuel cycle cost of 12.5 m/kWh (compared with 10 m/kWh used now) was proposed. The ranges of variables in LMFBR cost comparisons were used to indicate the uncertainty in fuel cycle costs.

3.3.2.5. Alternate Concepts. The objective of this task is to develop and evaluate alternate design concepts for the GCFR 300-MW(e) plant nuclear-steam supply system (NSSS) configuration, components, and related plant facilities and equipment.

3.3.2.5.1. Alternate Residual Heat Removal System Study

Following a review and evaluation of alternate concepts to enhance the RHR capabilities of the GCFR, three of the most promising were selected for more detailed investigation. These concepts were initially considered generic to the GCFR demonstration plant and the scaled-up commercial plant design, and the specific number of loops was not a major consideration. In certain RHR configurations, however, the number of loops could have a significant impact on the viability of the concept. The three concepts selected for further study included

1. A modified RHR system incorporating a standby CACS, with the water-side of the main loop upgraded to provide a safety class shutdown cooling system as a backup to the main loop in the event of loss of the main circulator and turbine trip. This design has been adopted as the main line design for the demonstration plant.
2. A design incorporating two 50% parallel or series flow circulators with the steam generator and CAHE in series.

3. A design in which a continuously operating boiling auxiliary heat exchanger is used in parallel with the steam generator. Steam from the CAHE drives a turbine generator set to produce in-house power for the electrically driven auxiliary circulators and other essential safety-related services. The potential for applying natural circulation cooling to a boiling CAHE incorporating a drum boiler was also to be investigated.

The major objectives of this study were to

1. Develop a reliable RHR system to provide the necessary levels of safety in accordance with preliminary safety and licensing criteria.
2. Provide, if possible, uninterrupted core cooling in the event of loss of main loop forced circulation.
3. Reduce the number of, or eliminate the need for, loop isolation valves.
4. Simplify the loop and PCRV geometry.

Natural convection cooling was not a mandatory requirement for the initial studies, but was considered in instances where it did not unduly complicate the total RHR system. Natural convection cooling via the CACS was assumed for the pressurized loss of forced cooling mode, although detailed analysis of natural circulation was not performed. The design parameters were based on a 300-MW(e) GCFR demonstration plant with three main and three auxiliary loops (Ref. 3-3).

Initially, the steam generators and the CAHE were to be located in a single cavity within the PCRV, with the CAHE placed below the steam generator bundle and functioning as an economizer for normal operation and as a CAHE for RHR. The apparent advantages of this arrangement were that it simplified the flow path and PCRV layout and possibly reduced the number of

helium valves. However, this arrangement was rejected on the grounds that both the main and auxiliary cooling modes were not sufficiently independent, as required by the safety criteria. In addition, residual heat in the core cannot be rejected in the passive natural convection cooling mode via the CACS loop because of the insufficient elevation difference between the CAHE and the reactor core.

An alternate RHR concept with the CAHE placed above the steam generator was also considered. This arrangement provided sufficient elevation between the CAHE and the reactor core to allow natural convection during pressurized cooldown. This concept required a complex flow path. First the coolant had to flow downward over the steam generator bundle. Then it had to flow upward through the vertical ducting, reverse, again flow downward over the CAHE bundle, and then return to the core inlet flow plenum via the circulators. This complicated flow path would have a substantial impact on the system pressure drop and the PCRV layout, including the isolation valve arrangement. To remedy these shortcomings, the RHR configuration was modified. For this parallel flow arrangement, the steam generator and CAHE were installed in one common cavity and the main and auxiliary circulators in another. The loop isolation valve was arranged to allow core outlet gas to be diverted to the main or auxiliary circulator via either the steam generator or the CAHE. This concept is essentially applicable to a parallel flow system, but has the same safety constraints as the series flow steam generators and CAHE arrangement. Based on the safety and PCRV layout considerations, these RHR concepts were not recommended for subsequent evaluation.

The concepts in the alternate RHR study are based on the following assumptions:

1. The CAHE should provide an independent cooling mode.
2. The CAHE and steam generator should be located in separate cavities to reduce component common mode failure interaction.

3. The auxiliary turbogenerator set driven by steam generated in each CAHE should be adequate to drive all essential service equipment, including all motor-driven circulators. Excess electric power could be made available to the grid.

Accordingly, four RHR concepts with different helium circulator drives and a parallel circulator arrangement were compared with the base case three-loop design (Table 3-1). The basic features of each concept, including heat exchanger and circulator arrangement, in-house generator set, etc., have been described along with their major advantages and disadvantages. Heat balance and plant efficiency have also been determined (Figs. 3-3 through 3-7).

The conclusions of this study are as follows:

1. The RHR concepts investigated to provide uninterrupted cooling do not have an advantage over the main loop cooling system with an integral shutdown cooling system and parallel CACS presently considered for the demonstration plant. However, a parallel flow steam generator and CAHE in conjunction with parallel flow circulators is recommended as a backup RHR system design.
2. Series flow helium circulators are not feasible for meeting the RHR normal and depressurized cooling modes specified in this study.
3. Parallel flow circulators are marginally acceptable for satisfying the RHR normal and depressurized cooling modes specified in this study, although not all aspects of running parallel flow circulators have been fully assessed.
4. The valve logic applied to the parallel and series loop concepts seems to offer no advantage in terms of reduction of the number of loop isolation valves per loop. On the contrary, additional valves would be required for some of the proposed RHR concepts.

TABLE 3-1
COMPARISON OF FOUR RHR CONCEPTS

	Base Case	Concept (a)			
		1	2	3	4
100% power					
Helium loop ΔP (MPa)	0.241	0.262	0.241	0.262	0.262
Total circulator power [MW(e)]	31	34	31	34	17 MW(e) + 16 MW(steam) = 33
Circulator power per circulator	10.1 MW	5.6 MW(e)	5.2 MW(e)	5.6 MW(e)	5.6 MW(e), 5.3 MW(steam)
Gross electrical output (MW)	331	299	306	321	310
Net electrical output (MW)	283	251	261	272	278
Net plant efficiency (%)	32.5	28.9	30.0	31.3	31.9
Circulator head (km)	3.05	3.32	3.05	3.32	3.32
DBDA at 4% power and 0.179 MPa					
No. of circulators operating	2 (auxiliary)	3	2	3	3
System ΔP (KPa)	10.34	11.03	10.34	11.03	11.03
Circulator water heat (km)	5.99	6.50	5.98	6.5	6.5
Flow per circulator (m ³)	35.82	23.79	35.82	23.79	23.79
Power per circulator (MW)	0.54	0.39	0.54	0.39	0.39, 0.37

(a) Concept 1 = series heat exchanger with parallel circulator; concept 2 = parallel, six loops, even flow; concept 3 = parallel, six loops, uneven flow; concept 4 = same as concept 3, but with steam-driven circulator.

Fig. 3-3. Alternate 1: series steam generators with parallel circulators

NET PLANT ELECTRICAL OUTPUT = 163.9 MW MAIN TG
 3 x 47.6 = 142.8 MW AUXILIARY TG
 -31.0 MW CIRCULATOR POWER
 -8.4 MW BFP
 -6.0 MW CIRCULATOR H₂O & HOUSE POWER (ESTIMATED)
 261.3 MW

NET PLANT EFFICIENCY = 261.3/870 = 30.0%

T = TEMPERATURE [°C (°F)]
 P = PRESSURE [MPa (PSIA)]
 W = FLOW [kg/h (LB/H)]
 h = ENTHALPY [kJ/kg (BTU/LB)]
 η = EFFICIENCY

TG = TURBINE GENERATOR
 STG = STEAM GENERATOR
 HTG = HEATING
 FW = FEEDWATER
 SCS = SECONDARY COOLING SYSTEM
 BFP = BOILER FEED PUMP
 ALC = AUXILIARY LOOP COOLER

Fig. 3-4. Alternate 2: six parallel, equal size loops with safety class turbine systems

Fig. 3-5. Alternate 3: three main loops with three auxiliary loops (uneven flow, two parallel circulators)

Fig. 3-6. Alternate 4: three main loops and three auxiliary loops with parallel circulators (one steam-driven)

NET PLANT EFFICIENCY = 283/870 = 32.5%

T = TEMPERATURE [$^{\circ}\text{C}$ ($^{\circ}\text{F}$)]
P = PRESSURE [MPa (PSIA)]
W = FLOW [KG/H (LB/H)]
h = ENTHALPY [kJ/KG (BTU/LB)]
 η = EFFICIENCY
TG = TURBINE GENERATOR

STG = STEAM GENERATOR
HTG = HEATING
FW = FEEDWATER
SCS = SECONDARY COOLING ,
SYSTEM
BFP = BOILER FEED PUMP
ALC = AUXILIARY LOOP
COOLER

Fig. 3-7. Base demonstration plant configuration with standby CACS loops

5. Continuous core cooling can be provided by either parallel or series heat exchangers in conjunction with parallel flow circulators. This represents the most significant feature of these concepts from a safety and reliability standpoint.
6. A passive CACS incorporating a boiling CAHE appears feasible, but additional analysis and evaluation is required before it could be recommended as a replacement for the present reference design.

3.2.2.5.2. Alternate Core Support Design Study

Alternate core support design concepts are being investigated as a backup for the single-piece, edge-supported grid plate core support for the GCFR demonstration plant. As a result of scale-up and core power density variation studies (Ref. 3-4), there is some concern with meeting the required natural frequency for an edge-supported grid plate which limits the seismic loadings in a commercial size plant. This condition is aggravated by additional limitations imposed on the single-piece grid design by forging technology size and weight restrictions. Five alternate core support concepts were evaluated. Concept A was based on a modified GBR-4 design (Ref. 3-5) wherein the reactor core is supported by multiple support pillars extending from and anchored to the PCRV core cavity bottom liner. Two concepts were based on a modified LMFBR core support design: one with a mechanical holddown (concept B), and another with a hydraulic holddown (concept C). The last two concepts were based on bottom-supported grid plate designs: one with a single plate (concept D) and another with a composite grid plate with two cylindrical supports (concept E). Figure 3-8 shows concept E, and Table 3-2 summarizes the major advantages and disadvantages of the concepts.

Fig. 3-8. Concept E: upflow core, bottom-supported grid plate

TABLE 3-2
SUMMARY OF THE MAJOR ADVANTAGES AND DISADVANTAGES OF FIVE
GRID PLATE SUPPORT ALTERNATIVES

Concept	Advantage	Disadvantage	Remarks
Concept A: modified GBR-4	Replacement compatible with cast-in-place core cavity closure design	Bypass flow at block interfaces (~2%); approximately twice normal bypass flow	Difficult fabrication requirements, but can be used for larger core designs
Concept B: modified LMFBR, three-plate edge support	Composite grid plate structure improves structural integrity with mechanical holddown	Complex composite structure	Difficult to fabricate, but could be designed with peripheral support
Concept C: LMFBR type design (CRBR); structural grid plate with clustered elements, edge plate support	Hydraulic holddown substantially improves grid plate structural integrity	Difficult locking and PES requirements	Applicable for larger reactor core designs; could possibly allow unloading and reloading of a block of seven assemblies at one time; peripheral support
Concept D: bottom-supported grid plate with outer ring; single support cylinder	Single-piece grid plate design and low ΔP penalty	Complicated support structure; reduced compatibility with core catcher requirements	Limited applicability; no room for reactor core growth
Concept E: composite bottom-supported grid plate with separate support, seal, and hold-down functions; two cylinder supports	Substantially improves grid plate structural integrity	Complicated composite grid plate design with two bottom support skirts	Applicable to larger reactor core designs with added loop pressure drop

Based on this study, the following conclusions and recommendations are made:

1. Two designs, (1) a two-assembly core support grid design supported from below by a single, large-diameter, cylindrical support structure (concept D) and (2) a modified LMFBR (CRBR) type edge-supported composite grid plate with clustered assembly modules (concept B) are considered feasible alternatives to the present single-piece, edge-supported grid plate design. Both concepts can be designed to be manufactured within the current fabrication forging limits, i.e., a 7.12-m diameter and a thickness of 0.61 m.
2. Core support concept D is recommended if a bottom-supported core is desired; concept B is recommended if an edge-supported core is desired. The choice of support system depends largely on CDA and postaccident fuel containment (PAFC) requirements.
3. A consideration with concept A is that it might lead to easing the requirement of having all internal components removable by not requiring a removable plug in the top of the reactor cavity.

3.3.2.5.3. Integral Reactor Closure Study

Alternatives to the fixed and rotating plug designs were considered for the reactor cavity closure for the GCFR demonstration plant and for scaled-up design applications. Two cast-in-place closure designs were developed: (1) one with a large plug to accommodate the longitudinal prestress system (vertical tendon system within the closure) and (2) another with a special shear anchor design at the interface between the plug and the PCR. The latter design would be required to limit the shear stresses in the grout between the plug and the PCR concrete during PCR pressurization. In both cases, the weight of the plug is excessive and difficult to handle. In addition, once the plug is integrally bonded with the PCR,

removal of it would be difficult, since chipping and removal of the grout would be required. Based on this study, the following conclusions were reached:

1. The cast-in-place closure, although feasible, does not have any major overall advantages over other designs and should not be pursued further except possibly to complete a more detailed cost benefit study.
2. If the removal of reactor internal components remains a requirement, it is recommended that the removable plug design be retained.
3. Since there are no feasibility concerns with a removable plug, there is no incentive for a cast-in-place alternative.

3.3.2.5.4. CACS Drum Boiler/Separator Study

A conceptual design and analysis of a steam separator was performed as part of an RHR study to investigate the feasibility of natural convection CACS cooling with a boiling CAHE (Section 3.3.2.2). Natural convection cooling on the primary side is available during pressurized core cooldown owing to an elevation difference between the CAHE and the reactor core. To establish natural convection cooling on the secondary side of the CAHE (water side), a drum boiler concept was proposed which would separate steam from wet steam generated in the CAHE. Because of the large density difference between the steam and the condensate, an adequate driving force would be created to initiate natural convection cooling, thereby rejecting the core heat to the environment via the ALC in a passive cooling mode. Separation tank dimensions are determined by the following criteria:

1. Steam/water droplet separation requires a maximum steam velocity of about 0.91 m/s, which necessitates a minimum tank diameter of 1.42 m.

2. Steam separation requires a minimum distance of 1.52 m between the steam outlet pipe (to ALC) and the tank water level during normal operation.
3. Prevention of steam entrainment requires a maximum water velocity of ~ 0.15 m/s in a down-comer region of the tank.
4. System water/steam expansion requires an ullage space of more than 11.61 m^3 .
5. For cold startup conditions a sufficient quantity of water, perhaps 1.52 m above the down-comer pipe, is required.

Based on the above criteria, the required tank dimensions would be $2.43 \text{ m } \phi$ x 6.1 m with a 2:1 ellipsoidal head on each end. After reviewing the steam conditions and the flow requirements during pressurized cooling, it was concluded that a simple separator tank without elaborate separation equipment would be required.

3.3.2.5.5. Reactor Core Flooding Study

A limited feasibility study of a last-ditch cooling system for the GCFR in the unpressurized refueling mode was completed. Passive last-ditch natural convection cooling, normally available during pressurized conditions, may not be available to cool the core following accidental loss of forced cooling during refueling operations owing to the low primary coolant system pressure. Consequently, an alternative mode of cooling is required to maintain the overall reliability of the reactor RHR system during refueling. A number of alternative core cooling methods which fulfill these requirements were identified, including a water flooding/spray system.

There are a number of fundamental objections to the water flooding/spray system, including the intentional introduction of water into the helium primary gas cooling system. However, these were set aside so that the merits, feasibility, and limits of the concept could be evaluated. The

study addressed thermohydraulic and recriticality effects resulting from introduction of water into the core. System requirements, conditions, and design possibilities, BOP interaction, and effects on reactor internals, were also investigated. Safety and reliability were not covered.

Figure 3-9 shows the flooding system. Each fuel blanket and shield assembly is internally sprayed to cool the core, preferably at an axial plenum location between the upper blanket zone and the upper shield. The coolant water is supplied from the bottom of the PCRV through a water header system mounted below the grid plate. The proposed concept will have a limited impact on the selection of a refueling scheme, except that mechanical connection during refueling would be more difficult. The connection between the fuel assembly and the water supply manifold interface is of particular concern. A number of different options were investigated, but all involve additional complexity for the core assemblies and the grid plate.

The use of boronated water in the spray cooling system presents problems of boron plateout outside the core region, with the possible risk of recriticality. The concentration of boron in the wet steam may not be sufficient to prevent recriticality. A number of alternative poisons have been identified, including silver, hafnium, and gadolinium, but additional work is required to verify their acceptability.

Although the water flooding system is considered feasible from a mechanical design point of view without regard to recriticality and water flow requirements. Safety, reliability, and testability considerations make this approach to core cooling undesirable. It is therefore recommended that a more complete investigation be made of the feasibility of providing natural convection gas cooling during refueling by repressurizing the PCRV with helium.

3.3.2.6. Two-Loop Demonstration Studies. A first attempt, based on HTGR data, to assess the impact of NSSS components on the probability of achieving design output has been completed. The tentative results show that the

Fig. 3-9. Reactor flooding design (within reactor core cavity)

circulator will require a 10% design margin to have 85% probability of success. Further studies will be required to establish design uncertainties and investigate their impact on the probability of success.

Earlier studies showed that the demonstration plant cannot achieve a prototypical steam temperature or flow rate, assuming that feedwater temperature and pressure are prototypical. New studies show that a prototypical steam temperature can be readily obtained by deviating from a prototypical feedwater temperature, and a prototypical steam flow can be achieved by modestly deviating from feedwater temperature and pressure. However, simultaneously obtaining both prototypical steam temperature and flow appears impractical.

The effect of decreasing the main helium circulator design speed from 3000 to 2400 rpm was evaluated. The motor efficiency increases about 1% and the compressor efficiency about 2%, but the compressor diameter increases 0.69 m. The overall cost impact is an increase of \$2.9 million (minimum) owing to the increased PCRV height for a horizontal circulator. Therefore, this change was not recommended.

3.3.2.7. Large Plant Scale-Up Studies. The objective of this task was to improve the tentative parameters of the demonstration plant reference design by trying to extend the current large-plant technical limits. Potential technical feasibility problems associated with the large plant design concept were identified and corrections sought. The scope of this task included

1. Configuration studies investigating areas in which the feasibility of the plant design is questionable or where an alternative arrangement offers an extension of a parameter limit.
2. Studies of large plant core and NSSS component limits in an attempt to extend the tentative parameters.

3. Analyses to define additional requirements or confirm the adequacy of the design.
4. Final parameter selection.

All studies, including final parameter selection, were completed. A general description of the studies and their impact on plant parameters follows.

3.3.2.7.1. Configuration Studies

Previous large plant work indicates that the core support structure design might result in a grid plate which exceeds current manufacturing limits. Recent studies indicate that reducing hole size (thereby increasing ligament efficiency), reducing conservatism regarding seismic events, and performing more detailed design and analysis will probably allow the use of a single-piece grid plate. If these efforts are unsuccessful, there are two alternatives: (1) grid plate manufactured from two or more pieces or (2) a cylindrical intermediate support which results in a thinner single-piece grid plate. Detail seismic analysis of the single-piece grid plate did not completely resolve its problems, and therefore grid plate design is still an open issue.

Since containment and confinement building feasibility had not previously been studied, a preliminary design was prepared for a vertically bottom-mounted circulator. There were no major problems associated with the design of these buildings. (This design was subsequently used to compare alternative PCRV concepts.)

3.2.3.7.2. Limit Studies

The core, steam generator, PCRV, CACS, and turbine generator were studied with the intention of extending the plant technical limits. Increases in helium pressure were investigated as a means of improving core performance at constant circulator power. However, because of current

material limits, the fuel rod-to-rod spacing is limited to a nominal 3 mm, which places an upper limit on achievable nuclear performance. The spacing limit results from rod bowing caused by differential swelling and is not related to any fabrication limit.

The best material expected to be available in the large plant is D9, a modified 316 stainless steel, which is being developed in the LMFBR program. D9 still has bowing problems which restrict the minimum rod-to-rod spacing, but they are fewer than those of 316 stainless steel. Ferritic steels, such as HT-9 and D57, which might eliminate this limit were considered, but there are inadequate data available for material properties above 650°C. Extension of the current data base to 750°C, the required GCFR limit, is beyond the present funding capabilities of the program.

The steam generator does not currently appear to be at a diameter or height limit. Detailed design work extended previously perceived limits. Given a helium-side pressure drop, heat duty, and helium temperature, a number of acceptable designs can be developed. A bimetallic weld is still required in the superheater outlet tubing. It is possible to reduce steam temperature to a point where a bimetallic weld is not required, but this is not recommended because of increased moisture in the low-pressure turbine generator exhaust. Reducing steam temperature also reduces plant efficiency.

An existing three-dimensional model of a 60-deg section of the down-flow three-loop GCFR demonstration plant PCR-V was modified to accommodate a horizontal circulator. Initial and EOL cases were run for primary coolant pressures of 9.3, 10, and 12 MPa. Local stresses in problem areas such as the horizontal circulator cavity and the containment mat can be reduced so that pressures up to 12 MPa are feasible.

A core cavity closure was designed and analyzed using two-dimensional elastic and ultimate stress techniques. This core cavity was chosen because it is the largest PCR-V cavity and hence has the most severe requirements for all the closures. It was concluded that a 12-MPa primary coolant pressure was feasible.

The heat transfer component sizes for the commercial plant CACS were developed for the most demanding event: spurious reactor trip with no loss of helium and core cooling provided by the CACS in the natural convection mode. Operation of these same components in the forced convection DBDA mode requires a 746-kW CACS circulator motor. The CACS design appears feasible, and no problems are expected with either the steady-state or transient conditions. With the exception of the auxiliary circulator compressor wheel, the major CACS components, such as the CAHE, motor, controller, and ALC modules, may be used in the demonstration and commercial plants without change.

A single 1800-rpm tandem-compound turbine generator appears feasible, although only one supplier is currently available. Twin turbines, or cross-compound machines, are also possible.

3.3.2.7.3. Confirming Analysis

A qualitative evaluation of large plant safety was conducted. The evaluation was based on a comparison with the three-loop demonstration plant design. The commercial plant is considered easier to license than the demonstration plant. The six-loop large plant RHR reliability/availability was also compared with the three-loop demonstration plant owing to the extensive work performed on that plant and its similarity to the commercial plant. When a statistically independent method is used and less than the full complement of loops is required, the large plant is better. If the current state-of-the-art of common cause analysis is used, common cause considerations reduce the benefits arising from different amounts of equipment, because redundancy of components receives little credit in common cause failure rates. It was concluded that the large plant has a small RHR reliability advantage over the three-loop demonstration plant. However, the magnitude of the difference is within the error band arising from modeling and data uncertainties.

Two types of availability analysis were identified: full-power availability (the availability of output at 100% rated power) and on-line

availability (the availability of any positive power output). For full-power availability, the fewer the number of helium loops, the better, because the annual frequency of failure is lower. The estimated full-power availability of the larger plant is 86% compared with 88% for the three-loop demonstration plant. Although no quantitative values have been developed for on-line availability, it should be better for the large plant, assuming that the plant can continue to operate with one or more loops out of service. A qualitative assessment of capacity factors indicates that the six- and three- loop plants are nearly the same. Note that the capacity factor is an important consideration, because capacity factor impacts operating costs in terms of the cost of replacement power.

The RHR system was also analyzed for transient response. Three events were considered: spurious trip with no loss of helium, slow loss of helium followed by reactor trip on low helium pressure, and rapid loss of helium with resultant reactor trip, i.e., DBDA. To provide adequate cooling during these events, the main loop cooling system, shutdown cooling system, and CACS with forced and natural convection were used in various combinations for eight cases. It was concluded that the CACS could fulfill its design function of providing adequate cooling for the fuel assemblies by forced convection and natural convection.

A DBDA with a $9.68 \times 10^{-2} \text{ m}^2$ hole, followed by reactor trip on low helium pressure, was run without exceeding full circulator speed, with adequate cooling provided by five main loops. Although using the main loops for cooling during a DBDA is not a current design requirement. This capability appears to exist if needed in the future for the commercial plant.

The refueling time for the large plant is approximately 37 days including plant cooldown and startup. This value is felt to be too large, although there is no optimum value for comparison purposes, because the large plant maintenance schedule has not been prepared. In the interim, the HTGR criterion of 21 days has been established as a goal.

The potential influence of commercialization on plant parameters was briefly examined. The normal design process ensures that demonstration plant performance will be conservatively specified. For example, GA has typically specified that performance warranties have an 85% to 90% chance of being met or exceeded. This is more than adequate to cover normal performance uncertainties, and barring a freak occurrence, components so specified will have extra capabilities. Parameters should be specified so that any extra capability can be used in the future. This would lead to larger circulator motors, higher helium pressures, and higher feedwater temperatures. These suggestions were made for commercialization reasons, but it is important to avoid technical limits and reduce development risk.

Advanced materials will be tested in the demonstration plant, but it appears that these materials will not be available for the first commercial plant; however, they may be available shortly thereafter. Therefore, it is necessary to design for the materials which will be available for the commercial plant. The advantages of advanced materials are expected to be in their nuclear characteristics and not in their high-temperature capability. Hence, parameter selection should not anticipate higher gas temperatures.

3.3.2.7.4. Parameter Selection

The selection of the large plant parameters involved not only technical considerations, but economic trade-offs as well. The studies described above and the SOFAST analysis (Section 3.3.2.4) resulted in a set of GA-recommended parameters, which were presented to Helium Breeder Associates (HBA). HBA expressed concern over the high helium pressure and suggested lower values for helium pressure, feedwater temperature, and core inlet temperature. A design approach which was acceptable to GA and HBA was established. This approach incurred minimal economic penalties compared with the original recommendation.

The helium pressures contemplated were higher than the current practice for gas-cooled reactors. The major concern with high helium pressure is that unforeseen structural design problems may arise. On the other hand, the high helium pressure appears to be necessary to meet GCFR nuclear performance goals with reasonable circulator power. Therefore, pressure levels approaching currently established limits (12 MPa) were selected.

Table 3-3 gives the final parameters, which are roughly the low helium pressure conditions suggested by HBA. However, for all affected structural parts, the design pressure is based on a helium operating pressure which is 0.5 MPa higher and which provides design margin. This creates a great deal of flexibility for conceptual design. It allows the higher pressure to be evaluated in detail and preserves its long-term potential. If the higher pressure proves to be impractical, the design pressure can simply be reduced to a more practical value, i.e., down to the 10.5-MPa operating helium pressure. Margin studies to be performed during conceptual design may show that retaining the 0.5 MPa as margin is cost effective, or the normal operating helium pressure can be increased up to 110 MPa to achieve the economic benefits of higher pressure. In neither case would the sizing work done for conceptual design be invalidated, and therefore the scheduled data for completing the conceptual design can be maintained. Table 3-3 shows system conditions using identical NSSS components at the higher operating helium pressure; these conditions roughly correspond to initial GA recommendations. (BOP design and perhaps circulator blading would change with the alternative conditions.)

If the higher pressure proves to be impractical, some of the components will be oversized; or if the alternate conditions with higher pressure are adopted, the system design conditions (steady-state and transient) will be slightly different. The impact will be modest in both cases and is acceptable, especially considering that the design will not be optimized for the conceptual design in any event. Nevertheless, some of the open issues will be resolved early in the conceptual design to minimize their impact.

TABLE 3-3
RECOMMENDED PARAMETERS AND ALTERNATIVE PERFORMANCE
WITH THE SAME NSSS COMPONENTS^(a)

	Recommended Operating Condition	Alternative Condition
Major recommended parameters		
Maximum cladding temperature (K)	1023	1023
Core outlet temperature (K)	830	833
Steam generator helium inlet temperature (K)	826	830
Turbine generator throttle pressure (MPa)	12.5	12.5
Steam generator outlet temperature (K)	783	783
Feedwater temperature (K)	464	480
Core inlet temperature (K)	575	583
Circulator power margin (%)	~30	~30
Helium operating pressure (MPa)	10.5	11.0
Additional helium operating pressure margin for structural design (MPa)	0.5	0
Resultant performance parameters		
Core doubling time (yr)	13.46	13.46
Circulator motor shaft output, including margin (MW)	22.4	22.4
Plant electrical rating (MW)	1291	1303
Plant efficiency (%)	35.9	36.2
Reactor pressure drop (kPa)	180.6	185.7
Total helium pressure drop (kPa)	229.6	235.8
Core helium flow rate (kg/s)	2722	2770
Breeding ratio	1.43	1.43
Core fission loading (kg)	5657	5657

^(a)All data are subject to minor modifications according to detailed heat balance calculations.

3.3.3. System Design (BOP)

3.3.3.1. Demonstration Plant. The demonstration plant conceptual design was updated, and design criteria for the helium purification system were developed, including module sizes, shielding specifications, and maintenance access requirements. Feasible locations for system components were investigated and it was determined that the space requirements are too great to permit housing inside the containment building. Alternate locations in the confinement or the reactor service building will require heavy shielding plus liners so that, in effect, such locations become extensions of the containment building.

Data were generated for the two-loop, 400-MW(e) demonstration plant containment building design. Volumes, surface areas, and heat sinks within the containment were calculated for use in DBDA transient analysis and incorporation in the RHR evaluation study.

Preliminary results obtained from the computer code CNTB (Ref. 3-6) give a containment peak back pressure of 0.47 MPa following a DBDA. This high back pressure precludes the use of a steel containment structure, since the plate thickness necessary to sustain the induced hoop stress exceeds 444.5 mm. Code requirements stipulate field annealing for fabrication of plates more than 444.5 mm thick. The cost of field annealing is prohibitive and time consuming and is therefore to be avoided whenever possible.

3.3.3.2. Commercial Plant. The vertical circulator concept was incorporated into the reactor containment/confinement building. It was decided that additional work should be done on the BOP design to determine the feasibility of pipe routing, separation, and restraint requirements.

Heat balances were revised as follows: the gross output increased from 1454 to 1485 MW, and the turbine heat rate decreased from 8695 to 8541 Btu/h. The 1.77% improvement in heat rate is due to a combination of the following three factors:

1. An increase in feedwater temperature from 188° to 218.5°C.
2. A decrease in feedwater pressure from 16.89 to 15.95 MPa.
3. An increase in the number of feedwater heaters from 4 to 6.

A study of the six-loop commercial plant was performed to determine the feasibility and problems involved in routing piping and electrical connections on the top of the core plug within the area of refueling penetrations.

A study was conducted to assess the advantages and disadvantages associated with a top exhaust steam generator compared with a bottom exhaust steam generator insofar as the BOP design is concerned. It was concluded that the bottom exhaust steam generator is the preferred configuration with regard to costs, safety, and accessibility.

BOP requirements for liner cooling water headers for the six-loop commercial plant were identified. The main steam and feedwater lines for the commercial plant with a bottom exhaust steam generator were analyzed for thermal stresses to determine the feasibility of routing those lines inside the reactor containment building. The results of the analysis indicate that the main steam lines are slightly overstressed; however, this condition may easily be corrected by increasing the legs of the expansion loop. The feedwater lines are well within the allowable stress limits.

A study was initiated to investigate the maintenance capability (removal/replacement) of the main motors for the horizontal circulator configuration in the GCFR commercial plant. From the work thus far completed, it has been determined that the initial "dog house" concept does not provide adequate space for the removal of the motors through the

annular space between the PCRV and containment wall. An alternate scheme has been devised in which the motors are transferred in an annular corridor by extending the base of the containment building. The new scheme and building modifications will be developed further.

3.3.4. Seismic Engineering

There was no activity on this task during this quarter.

3.3.5. Materials Engineering

The major effort during this quarter was aimed at updating GCFR structural component radiation damage data requirements, assigning key component fluence/temperature limits, and coordinating the EBR II testing program.

3.3.5.1. Updating of Radiation Damage Data Requirements. A change was made from a downflow to an upflow mode. Reference 3-7 gives the material irradiation effects data requirements for GCFR out-of-core structural components. Table 3-4 outlines current design conditions with respect to temperature and maximum fluences for key GCFR metallic components. The required irradiation effects property data requirements were updated and are listed in Tables 3-5 and 3-6.

The general requirements in Ref. 3-7 are still in effect. For example, the highest priority need is high-temperature (500° to 650°C) data on 316 stainless steel (reference structural alloy) and its weldments. Ductility is the property of primary interest. Other baseline and alternative materials include Hastelloy X, carbon steel, 304 stainless steel, alloy 800H, In-718, 12 Cr steel, and 9 Cr steel. The recommended irradiation exposure and property requirements for these materials were revised and are listed in Table 3-5, which also gives the tests covered by the national program. Table 3-6 gives priorities to the materials and property requirements not covered by the national program but needed for design and verifications.

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

	Applicable Component	Maximum Metal Temperature (°C)	Maximum Total Fluence (nvt)
Primary coolant system boundary	PCRv (concrete, liner, etc.), reactor cavity 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	579°C	5 x 10 ²³
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	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, In 718, HT-9 (12 Cr steel).

TABLE 3-5
IRRADIATION EFFECTS DATA REQUIREMENTS FOR GCFR STRUCTURAL
ALLOYS FOR THE DEMONSTRATION AND COMMERCIAL PLANTS

Priority	Material	Irradiation and Test Temperature Range (°C)	Total Fluence Range (n/cm ²)	Fast Flux Fraction Range ^(a)	Displacements per Atom Range	Required Properties ^(b)									
						S	T	HCF	LCF	CF	SR	IRC	K	da/dn	C _v
1	316	500-650	1×10^{21} - 5×10^{22}	0.04-0.60	0.1-9.0	(R)	R	R	R	R	R	R	(R)	R	D
	316 welds	500-650	1×10^{21} - 5×10^{22}	0.04-0.60	0.1-9.0	(R)	R	R	R	R	R	R	(R)	R	(D)
	316	300-500	1×10^{21} - 5×10^{22}	0.1-0.40	0.1-6.0	-	(R)	R	(R)	R	R	R	(R)	R	D
	316 welds	300-500	1×10^{21} - 5×10^{22}	0.1-0.40	0.1-6.0	-	R	R	R	R	R	R	(R)	R	(D)
2	304	300-500	5×10^{19} - 5×10^{21}	0.05-0.40	-1.0	-	(R)	R	R	R	R	R	(R)	(R)	D
	304 welds	300-500	5×10^{19} - 5×10^{21}	0.05-0.40	-1.0	-	(R)	R	R	R	R	R	(R)	(R)	D
	718 (7C6)	500-650	1×10^{21} - 2×10^{22}	0.05-0.60	0.1-3.0	(R)	(R)	R	(R)	(R)	(R)	R	(R)	(R)	(D)
	718 welds	500-650	1×10^{21} - 2×10^{22}	0.05-0.60	0.1-3.0	(R)	(R)	R	(R)	(R)	(R)	R	(R)	(R)	(D)
	HT-9	500-650	1×10^{21} - 2×10^{22}	0.05-0.60	0.1-3.0		R	R	(R)	(R)	(R)	R	(D)	(D)	(D)
	Hastelloy X	500-650	1×10^{18} - 5×10^{19}	0.15-0.40	0.001-0.005		R	R	(R)	(R)	(R)	R	(D)	(D)	(D)
3	316	500-650	1×10^{18} - 5×10^{19}	0.15-0.40	0.001-0.005	-	R	R	R	R	R	R	(D)	(D)	D

(a) Fraction of flux with $E > 0.1$ MeV.

(b) TC = thermal conductivity; S = swelling; T = tensile stress; HCF = high-cycle fatigue; LCF = low-cycle fatigue; CF = creep fatigue; SR = stress rupture (includes minimum creep rate); IRC = in-reactor creep; K = fracture toughness; da/dn = fatigue flaw growth; C_v = Charpy V notch impact. R = data required for design or design verification, D = data desirable for safety/reliability analysis; (R) = adequate information from available data or current tests; (D) = adequate information from planned but not budgeted tests; (R) + (D) = (R) + (D).

TABLE 3-6
 ADDITIONAL MATERIAL IRRADIATIONS AND PROPERTY TESTS REQUIRED FOR GCFR
 STRUCTURAL COMPONENTS

Priority	Material	Temperature Range (°C)	Fluence Range (n/cm ² , E > 0.1 MeV)	Fast Flux Fraction Range ^(a)	Required Tests
1	316, 316 welds	500-650	1 x 10 ²¹ -5 x 10 ²²	0.04-0.60	Tensile, low-cycle fatigue, stress rupture
2	316 welds	300-500	1 x 10 ²¹ -5 x 10 ²²	0.1-0.4	Tensile, low-cycle fatigue, in-reactor creep
3	316, 316 welds	500-650	1 x 10 ²¹ -5 x 10 ²²	0.04-0.60	Creep-fatigue, in-reactor creep, high-cycle fatigue
4	316, 316 welds	300-500	1 x 10 ²¹ -5 x 10 ²²	0.1-0.4	High-cycle fatigue
5	304, 304 welds	300-500	5 x 10 ¹⁹ -5 x 10 ²¹	0.05-0.40	High-cycle fatigue
6	HT-9	500-600	1 x 10 ²¹ -2 x 10 ²²	0.05-0.60	Tensile, low-cycle fatigue, stress rupture
7	718, 718 welds	300-500	5 x 10 ²¹ -2 x 10 ²²	0.05-0.60	High-cycle fatigue
8	Hastelloy X	500-650	1 x 10 ¹⁸ -5 x 10 ¹⁹	0.15-0.40	Tensile, low-cycle fatigue, stress rupture
9	316, 800H, HX	500-650	1 x 10 ¹⁸ -5 x 10 ¹⁹	0.15-0.40	Tensile, low-cycle fatigue, stress rupture
10	316, 800H	500-650	1 x 10 ¹⁸ -5 x 10 ¹⁹	0.15-0.40	High-cycle fatigue, in-reactor creep, creep fatigue

(a) Range given is for actual component service conditions. It is not possible to attain high fluence exposures in soft spectra with existing facilities.

3.3.5.2. Radiation Damage Fluence/Temperature Limits. Radiation damage of metallic materials for out-of-core components in the GCRR was assessed using a criterion similar to that for CRBR structural materials, i.e., a residual total elongation (RTE) of 10% (minimum EOL residual ductility). The components assessed included a thermal barrier (in-core cavity), core barrel, upper radial shielding and assembly attachments, grid plate, core support cylinder, and shield assemblies and attachments. The reference material for these components was annealed 316 stainless steel. Hastelloy X is being considered for certain thermal barrier components which may be subjected to higher temperatures for longer times during accident conditions. 304 stainless steel, which is an alternative material for many of the above components, was also assessed.

Based on the available irradiation data, a 10% RTE curve for annealed 316 stainless steel was plotted on a temperature-fluence map (Fig. 3-10), and the safe region for 316 stainless steel was defined based on the 10% RTE criterion. As illustrated in Fig. 3-10, with the current design temperatures and fluences, annealed 316 stainless steel is satisfactory for all the above-mentioned components except exit shields. Figure 3-10 also suggests that the current design fluences for some of the high-temperature (such as core barrel, upper radial shielding and assembly attachments, fuel unlocking mechanism penetration ports, and upper radial shields and attachments) can be increased by an order of magnitude to 1×10^{22} nvt. However, the current available high-temperature ($\geq 550^\circ\text{C}$) irradiation data are relatively sparse. The on-going EBR-II tests will provide additional high-temperature data. An increase of the design fluences for these high-temperature components may be possible if the additional high-temperature data confirm the present 10% RTE curve.

The current design fluence for the exit shields exceeded the 10% RTE limit of 316 stainless steel (Fig. 3-10). It is thus necessary to account for radiation damage with regard to tensile properties, fatigue/creep-fatigue interactions, creep rupture, and other design properties. Furthermore, the alloy may exhibit significant swelling.

Fig. 3-10. Design temperatures, fluences, and 10% RTE of annealed 316 stainless steel for various out-of-core component applications

According to Ref. 3-8, annealed 316 stainless steel swelled in the temperature range 450° to 650°C (peak swelling temperature ~500° to 600°C), with a threshold fluence of approximately 3×10^{22} n/cm² ($E > 0.1$ MeV). Hence, it is apparent that the alloy would exhibit no significant swelling for all the components being assessed except the exit shields. The exit shields may exhibit significant swelling under current design conditions. Nevertheless, the percentage of swelling cannot be estimated without understanding the energy spectrum of neutrons expected for the exit shielding. This problem must be resolved.

With regard to Hastelloy X, irradiation data pertinent to GCFR conditions were sparse. Reference 3-9 shows that the tensile elongation at 649°C was reduced from 29% (control specimens) to 23% after being irradiated at 616°C to a total fluence of approximately 10^{20} nvt (5×10^{19} nvt for fast neutrons and 4.5×10^{19} nvt for thermal neutrons). A relatively mild reduction in tensile ductility at this fluence level (approximately an order of magnitude higher than the design fluence) was observed. No reduction in tensile elongation was observed after specimens were irradiated at 540°C to a total fluence of 1×10^{20} nvt (7×10^{19} nvt for fast neutrons and 3×10^{19} nvt for thermal neutrons). Based on these limited data, Hastelloy X appears to be adequate for thermal barriers under current design temperature and fluence conditions.

Helium generation in Hastelloy X due to B-10 (n, α)Li reaction is strongly dependent on the boron content in the alloy. The alloy normally contains about 20 ppm of boron. The material specification (AMS 5536), however, specifies a maximum boron content of 100 ppm. Thus, the calculation should be made on the production of helium atoms in Hastelloy X with various boron contents at the design fluence of 1×10^{19} n/cm² with pertinent neutron spectra. This is needed to check whether the criterion of 1 appm allowable helium gas concentration is met for Hastelloy X.

Figure 3-11 shows residual elevated temperature tensile elongation data for irradiated 304 stainless steel as a function of test temperature and total neutron fluence. A 10% RTE curve for annealed 304 stainless steel was established in the temperature fluence map. This curve is

Fig. 3-11. RTE of annealed 304 stainless steel as a function of temperature and total fluence (irradiation temperature = test temperature $\pm 50^{\circ}\text{C}$)

similar to that of annealed 316 stainless steel except at high temperatures ($\geq 550^{\circ}\text{C}$). At temperatures $\geq 550^{\circ}\text{C}$, 304 stainless steel exhibits a lower fluence limit compared with 316 stainless steel. Nevertheless, 304 stainless steel is satisfactory for all out-of-core components except exit shields.

3.3.5.3. EBR-II Structural Materials Irradiation Test. The GA-HEDL program to obtain near-term irradiated high-temperature 316 stainless steel tensile data for establishing fluence limits for GCFR shielding design continued during this quarter. Weldments prepared by GA were sent to HEDL, where test specimens and irradiation capsules were fabricated. Irradiations in EBR-II have been initiated.

Table 3-7 shows the finalized test matrix. A total of 72 tensile specimens will be irradiated in row 7 of EBR-II, including both parent and weld metal specimens at 550° and 650°C to fluences of $2 \times 10^{22} \text{ n/cm}^2$ ($E > 0.1 \text{ MeV}$). Weldments for the test program were prepared at GA from base material provided by HEDL which consisted of twelve 25.4-mm-thick 316 stainless steel plates measuring 152 x 610 mm. Five weldments, each measuring approximately 321 mm wide and 610 mm long, were prepared. Three different welding processes were used: shielded metal arc (SMAW), submerged arc (SAW), and gas tungsten arc (GTAW). Welding filter materials which met the requirements of RDT M1-1T and RDT M1-2T were used. The flux used for SAW was Arcosite S16. Nonconsumable backing bars machined from the 316 stainless steel were used for each weldment.

Table 3-8 lists the procedures used to prepare these weldments. High-quality welding practices were employed, i.e., grinding each pass to remove all traces of oxide and flux, grinding all starts and stops, and using a maximum interpass temperature of 177°C .

Figure 3-12 shows an end view of the joint preparation and fit-up used for all weldments. Each plate was preset to an angle of $\sim 20^{\circ}$ to compensate for the distortion expected from welding. Typically, after approximately one-third to one-half of the total number of passes were used for

TABLE 3-7
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 filler, two replicates.

TABLE 3-8
SUMMARY OF PERTINENT INFORMATION ON WELDMENTS PREPARED FOR
OUT-OF-CORE MATERIAL IRRADIATION EXPERIMENTS

Test Plate No.	Welding Process	Welding Procedure	Welding Parameters					Filler Material		Nondestructive Examination	
			Volts	Amps	Travel Speed (mm/min)	Wire Feed Speed (mm/min)	No. of Passes	Diameter (mm)	Type/ Heat No./ Supplier	Dye Penetrant	Radiography
1	SMAW	GGA-WS-133	23	135	210	—	60	3.2	E316-16/ 06814/ Teledyne- McKay	NSI ^(a)	NSI
2	SAW	GGA-WS-129	32-34	260	203	4318	12	1.6	ER-316/ 613144/ Johnston	NSA	Slag indications: 12.7 mm long at No. 2 edge; 6.35 mm long, 57.2 mm from No. 2 edge
3	SAW	GGA-WS-129	32-34	260	203	4318	22	1.6	ER-316/ 613144/ Johnston	NSI	NSI
4	GTAW	GGA-WS-132	20	130	114	508	46	1.6	ER-316/ 613144/ Johnston	NSI	Lack of fusion for 218-mm length starting 203 mm from No. 0 edge and ending 188 mm from No. 2 edge
5	GTAW	GGA-WS-132	20	130	114	508	57	1.6	ER-316/ 613144/ Johnston	NSI	NSI

(a) NSI = no significant indications.

(b) Used with Arcosite S16 flux, lot 5M6F.

Fig. 3-12. Joint preparation and fit-up for out-of-core material irradiation experiment weldments

any given plate, the plates had to be fillet welded to a backing plate to prevent excessive distortion. This was done most successfully by welding just before the plates moved to form a horizontal plane. A wide root opening was selected to provide a weld metal width of at least 25.4 mm.

Table 3-8 gives the specific welding parameters used for each welding process. Since selection of filler materials was limited to those available at GA that had been qualified to RDT specifications, smaller than optimum diameters were used for the SMAW and SAW weldments. Consequently, the number of passes indicated for these two welding processes were higher than their would have been if the filler material diameter had been optimized.

Liquid penetrant testing was done on the joint preparation surface, root passes, and final passes. There were no significant results for any of the plates or weldments. Radiography was performed to obtain information on the completed weldments after the nonconsumable backing bar had been machined off. The weldments were shipped to HEDL for test specimen fabrication. HEDL fabricated the test specimens from three heats of parent metal and three types of weldments prepared at GA. Subcapsules (which are the temperature-controlling vehicle for the irradiation capsules) were designed, and three complete subassemblies, each containing one of the three rods (24 specimens per rod), were fabricated and shipped to EBR-II for insertion in row 7. Unirradiated characterization of the parent metal heats was initiated. Figure 3-13 shows the schedule for the remainder of the program.

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 DOE-sponsored GCFR 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,

Fig. 3-13. EBR-II material irradiation schedule

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 core response 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.
4. The primary containment technology task is concerned with the response of the PCRV to the conditions established at permanent core subcriticality 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, and to ensure protection of secondary containment integrity. Furthermore, this task will establish a mechanistic basis to demonstrate the conservatism of the secondary containment site suitability source term.
5. The secondary containment technology task focuses on the performance of the secondary containment as a barrier to the release of radioactivity. The objective of this task is to quantify the secondary containment response 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. Furthermore, this task will ensure that the radiological consequences of a site suitability source term release to the containment are within 10CFR100 guidelines.

Work is currently in progress on all the above tasks. Logical probabilistic methods are employed to determine the probabilities associated with various accident initiation and progression sequences and to identify potential design modifications that would help reduce risks. 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 leading 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 object of current investigations. Postaccident fuel containment 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

This task is concerned with the technical definition of the overall GCFR safety program and the technical coordination of GCFR safety-related work being performed by GA, the national laboratories, and international participants of the GCFR program under the Umbrella Agreement.

During this quarter, the major objectives for FY 80 were determined:

1. Safety program definition. The GCFR safety program plan will be prepared for the upflow core design.
2. Probabilistic technology.
 - a. A GCFR plant specification for safety system reliability will be prepared. This specification will implement the engineering reliability integration plan and define the

objectives, responsibilities, procedures, analytical methods and data to be uniformly used for integrating safety system reliability targets into the design process.

- b. An RHR reliability report will be prepared for the new RHR system reference design in the upflow core.

3. Core accident technology.

- a. An integral analytical capability will be developed for the unmitigated LOSC accident sequence.
- b. A natural circulation model will be developed for the core cavity as part of the LOSC analysis code system.
- c. Design concepts capable of mitigating the consequences of LOSC accidents at the core level will be generated.
- d. The SMART test program will be defined for the upflow core.
- e. Analytic support will be provided for the FLS-3 experiment at LASL.

4. Primary containment technology.

- a. Molten fuel penetration into the PCRV concrete base slab will be assessed, and the feasibility of molten fuel containment within the PCRV concrete or on the containment base mat will be examined.
- b. Core energy release effects inside the PCRV will be studied.
- c. Fuel and fission product aerosol behavior after release from the core will be investigated.

5. Containment technology.

- a. Radiological containment analysis will be performed for the site suitability source term to support conceptual design.
- b. Parametric radiological consequence analyses will be performed for beyond design basis events.
- c. Consequence-mitigating features for the containment will be evaluated.

3.4.2. Probabilistic Technology

3.4.2.1. Residual Heat Removal Reliability for Two-Loop Demonstration Plant Configurations. Reliability analyses for the two-loop demonstration plant using commercial plant size loop components were continued. The results in Ref. 3-10 assumed statistically independent failures and immediate repressurization. During this quarter, common cause failure estimates were completed, and a delayed repressurization option with a 5-h forced convection period was evaluated.

Common cause failure estimates were performed using the β -factor method (Ref. 3-11). Generic β -factors of 0.1 for demand failure probabilities and 0.01 for running failure probabilities were used. Common cause was assumed within each of the systems, but statistical independence was assumed between systems.

The heat removal train and electrical system for the following configurations were analyzed:

1. Integrated two-loop main cooling system/shutdown cooling system with three CACS loops; forced convection, and natural convection in the CACS loops.

2. Two main cooling system loops, two CACS loops, and two independent shutdown cooling system loops; only natural circulation capability in the shutdown cooling system loops.
3. Two main cooling system loops, two CACS loops, and two independent SCS loops; both forced and natural circulation capability in the shutdown cooling system loops.

Table 3-9 presents the results of the analysis. The common cause failure assumption substantially limits the gain in RHR reliability achieved with repressurization because for all natural circulation modes, the auxiliary loop isolation valves and auxiliary loop cooler dampers must open to initiate natural circulation, and secondary side pressure control must be maintained for both the forced and natural circulation modes. For delayed repressurization and no repressurization, the CACS failure probability is dominated by failure to start and operate in the forced convection mode. Reduced dependence on functioning equipment to establish and maintain natural circulation would decrease the RHR failure probability with immediate repressurization. Configuration 2 includes two separate shutdown cooling system loops with only natural circulation capability. Without immediate repressurization, this configuration is rather unreliable because the shutdown cooling system cannot be used for depressurized cooling. Configuration 3 has two separate shutdown cooling system loops with both forced and natural circulation. The reliability of this configuration for RHR is very good.

3.4.2.2. Auxiliary Loop Isolation Valve Common Cause β -Factor and Various Main Loop Isolation Valve Requirements. The current auxiliary loop isolation valve (ALIV) is basically a two-sided check valve with a common hinge pin. It has the capability of opening by gravity or by forward flow. The two halves are normally closed owing to the reverse pressure from the main circulators. The demand failure rate for one-half of the valve is assumed to be 1×10^{-3} based on British CO₂ valve failure data.

TABLE 3-9
COMMON CAUSE RELIABILITY ESTIMATES FOR RHR (HEAT REMOVAL
TRAINS AND POWER SUPPLIES) FOR THREE CONFIGURATIONS
OF THE TWO-LOOP DEMONSTRATION PLANT

Configuration(a)	Failure Probability Per Year		
	No Repressurization	Delayed Repressurization	Immediate Repressurization
Integrated 2 MCS/SCS and 3 CACS loops (FC + NC in CACS)	3.0×10^{-5}	2.8×10^{-5}	1.3×10^{-5}
Independent 2 MCS, 2 CACS, and 2 SCS loops (only NC in SCS)	1.9×10^{-4}	1.4×10^{-4}	1.8×10^{-7}
Independent 2 MCS, 2 CACS, and 2 SCS loops (FC + NC in SCS)	3.4×10^{-7}	2.2×10^{-7}	1.5×10^{-7}

(a) MCS = main cooling system; SCS = shutdown cooling system; CACS = core auxiliary cooling system; FC = forced convection; NC = natural convection.

Opening of one-half of the valve is sufficient for CACS operation, and therefore a common cause failure of both halves of the valve is required to disable the valve. The probability of this occurrence is assumed to be $\beta \times 1 \times 10^{-3}$, or 1×10^{-4} per demand. In all previous analyses of common cause CACS failures, a separate common cause failure was assumed to be required to fail all three ALIVs completely, i.e., $\beta \times 1 \times 10^{-4}$, or 10^{-5} /demand. This assumption may be optimistic owing to the common environment of all six ALIV half-valves. The assumption that a single common cause failure would disable all six ALIV half-valves was tested, and the results are shown in Table 3-10. The RHR reliability is only reduced by a factor of 1.1 to 1.3 by this assumption and is thus not considered to have a major impact on the RHR reliability of the current reference design.

Table 3-10 shows the effects of three MLIV requirements: (1) both MLIVs must always close; (2) if one MLIV fails to close, three of three CACS loops are required for natural circulation; (3) if both MLIVs operate, at least two of three CACS loops are required for natural circulation. Hence, the main loops must be designed to self-isolate under natural circulation conditions. All previous analyses were based on case 2 assumptions. This analysis was performed for the current reference design (two integral main cooling system/shutdown cooling system loops and three CACS loops with forced circulation and natural circulation in the CACS and delayed repressurization).

The RHR failure probability per year is reduced by a factor of 5.5 to 6.8 if the main loops are self-isolating compared with all MLIVs closing for natural circulation. However, if a single failure of either an MLIV or a CACS loop can be accepted for natural circulation, then the benefit of main loop self-isolation is reduced to a factor of 1.2 to 1.3. The common mode failure assumptions for the ALIV have only a minor impact on overall RHR reliability.

TABLE 3-10
EFFECT OF MLIV AND ALIV REQUIREMENTS ON COMMON CAUSE RHR
FAILURE RATE (HEAT REMOVAL TRAIN AND ELECTRIC SUPPLY) FOR TWO-LOOP
DEMONSTRATION PLANT (a)

MLIV Requirements	Failure Probability Per Year		
	ALIV Common Cause β -Factor Used Twice(b)	ALIV Common Cause β -Factor Used Once	ALIV β -factor Δ
Case 1: both MLIVs must close	1.5×10^{-4}	1.6×10^{-4}	+1.1
Case 2: with 3/3 CACS loops, one MLIV must close, or with 2/3 CACS loops, both MLIVs must close	2.8×10^{-5}	3.5×10^{-5}	+1.3
Case 3: MLIV not required to close	2.2×10^{-5}	2.9×10^{-5}	+1.3
Case 1/case 3	6.8	5.5	
Case 2/case 3	1.3	1.2	

(a) Configuration: two integral main cooling system/shutdown cooling system loops; three CACS loops with natural circulation; delayed depressurization.

(b) Once for intraloop redundancy and once for intersystem redundancy.

3.4.2.3. Testing and Idling of Core Auxiliary Cooling System Loops for Controlled Depressurization Events. To reduce the RHR failure probability per year with delayed repressurization, a reliability estimate was made with the following assumptions for controlled depressurization events:

1. Start and test the CACS after shutdown but before PCRV depressurization while the natural circulation CACS is available in the primary coolant. With the PCRV pressurized, repair CACS components outside the PCRV if required. To repair CACS components inside the PCRV, the PCRV must be depressurized. Thus only the main cooling system/shutdown cooling system is available for cooling during this repair. The CACS repairs are assumed to take place before repair of the initiating event failure is started.
2. Idle the CACS auxiliary circulator during the total outage time.
3. Depressurize the PCRV.

The configuration analyzed is the current reference design with two integral main cooling system/shutdown cooling system loops and three CACS loops with forced and natural circulation and delayed repressurization. The ALIV β -factor was conservatively used only once. The results are compared with those for the standby CACS option was assumed in previous analyses. The overall RHR failure probability was reduced from $3.5 \times 10^{-5}/\text{yr}$ to $2.1 \times 10^{-5}/\text{yr}$ (a factor of 1.7) for the test and idle CACS option compared with the standby CACS option.

3.4.2.4. Evaluation of the Six-Loop Commercial Plant Design. A preliminary qualitative reliability analysis for the six-loop large commercial plant was performed. In this design, the main cooling system contains two feedwater trains, six steam generators, and six electric-motor-driven main circulators. The shutdown cooling system is a three-loop system in which each loop contains one flood-out pump and tank, one shutdown feedwater pump, two steam generators (shared with the main cooling system), two main circulators (also shared with the main cooling system, but driven by pony

motors with safety-grade electric power supplies), and one air-water cooler. The CACS consists of three independent loops, each of which is capable of cooling by forced or natural convection in the primary, secondary, or tertiary coolants.

The large six-loop commercial plant was compared with the old three-loop demonstration plant. A qualitative reliability evaluation indicated that although the six-loop configuration undoubtedly has a higher reliability, the difference in the reliabilities of the two plants may not be very large because of the equalizing effect of common cause failures considered within the framework of the current state-of-the-art. In addition, doubling of equipment (to provide six loops instead of three) occurs for only a few items (basically the steam generators and main circulators); the bulk of the equipment is not doubled.

There are two basic types of availability: one associated with full-power output, and one associated with any net power output. The latter was not studied, but a preliminary quantitative estimate of the former was made for comparative purposes. The simplified quantitative evaluation of full-power availability indicated a relative increase of about 24% in the unavailability of the six-loop plant over that of the three-loop plant. This increase was attributed to two factors related to the previously mentioned doubling of equipment: (1) the increased frequency of loss of full-power operation due to the increased potential for equipment failure and (2) the relatively large restoration times associated with some of the equipment failures.

A qualitative comparison of capacity factors for the two plants revealed that the capacity factors should be about the same, although various operational and procedural constraints (such as technical specification limits) could cause the capacity factor for one or the other of the plants to be higher. The near equality of the capacity factors seems more important than the above-noted difference in availabilities because of the direct impact of the capacity factor on the operating cost of the

plant. Although the near equality of the capacity factors may seem intuitively unreasonable, the logic of supporting approximate equality can most easily be illustrated in terms of the twin turbine generator concept versus the single turbine generator concept: let P be the nominal full-power output for both concepts and λ and T be the annual failure rate and restoration time (in hours), respectively, for a single turbine generator. For the single turbine-generator concept, the average annual down time (hours) resulting from turbine generator failures is λT . Each time the unit goes down, the power loss is P . The total average power-hour loss each year would be $P\lambda T$. For the twin turbine generator concept, the frequency of loss of a unit is 2λ , which gives an average down time of about $2\lambda T$. With only one unit down, the power loss would be $P/2$. Hence, the total average power-hour loss each year would be about $P\lambda T$, i.e., the same as for the single turbine generator concept.

To enhance RHR reliability, the twin turbine generator must be as functionally independent as possible. If the twin turbine generators are operated dependently (i.e., one turbine generator trip causing the other to trip), on-line availability and RHR reliability will be reduced by a factor of about two for events initiated by a single turbine generator trip.

Assuming statistical independent estimates, the current heat removal train and electrical supply reliability analysis indicates that the greatest unreliabilities occur when the PCRV is depressurized, since a turbine generator trip and/or outage does not require depressurization. Hence, the reliability improvements may not significantly improve the overall RHR reliability. However, for common cause failures, the turbine generator trip events seem to be among the dominant initiating events (not the most dominant), and thus some improvement in overall RHR reliability should be realized with a twin turbine generator option.

RHR reliability following loss of off-site power can be enhanced with the twin turbine generator option if both turbine generator sets are ramped back to maintain house load jointly. However, to accomplish this gain, failure of one of the turbine generator sets to run back to house load must

be compensated for by the other set; i.e., the portion of the house load from the failed turbine generator must be picked up by the second turbine generator. This option increases the availability of the main cooling system for RHR following loss of off-site power.

3.4.2.5. Reliability Data Development. Shutdown summary data have been compiled for Fort St. Vrain.

3.4.2.6. Engineering Reliability Integration Plan. A plan to integrate the reliability and design functions in the GCFR program has been initiated. The main purpose of the reliability program has been to improve reactor safety rather than plant availability. Application of reliability methods early in the design can be extremely beneficial in improving reactor safety, and the first qualitative steps of reliability analysis can be done more efficiently by the system or component designer rather than the reliability expert. The main purpose of the GCFR reliability integration plan is to involve the system/component designer in the reliability process by requiring that he perform the qualitative reliability analysis. The main impact of the plan on the GCFR program will be as follows:

1. The designers of safety systems and/or components will be required to do the qualitative reliability analysis by performing failure modes and effects analyses (FMEA) and fault tree analyses.
2. The qualitative analyses will be reviewed and follow-on quantitative analyses performed. The analyses will be integrated into the safety analysis reports, and system reliability will be compared with licensing goals.
3. Reliability analyses will be implemented during the conceptual and preliminary design phases so that there is flexibility to make changes if needed.
4. Reliability analyses will be performed on the final design to verify that reliability goals will be met.

The NRC has indicated that reliability analyses will be required for GCFR systems which have an impact on reactor safety. These systems are the plant protection system (PPS), reactor vessel (PCRv), and RHR system. Other systems having a significant impact on reactor safety, e.g., the reactivity control system, the fuel assembly PES, the reactor internal structures, may be added in the future.

3.4.3. Core Accident Technology

The core accident technology program will investigate the following:

1. Unmitigated GCFR response to events involving loss of coolable core geometry.
2. Specific design features which may prevent or reduce the consequences of the LOsC, loss of flow (LOF), transient overpower (TOP), or flow blockage accidents.

This approach was initiated as a result of a core accident technology program reevaluation after the decision for an upflow, bottom-supported GCFR core. Work during this quarter emphasized justification of the SMART experimental program and development of analytical methods for the FLS and SMART experiments at LASL.

3.4.3.1. Accident Prevention and Mitigation. A multilevel approach has been initiated to investigate options for reducing the consequences of an LOsC accident. Five levels are being considered:

1. Prevention of cladding melting (core damage prevention) upon failure of all RHR systems.
2. Prevention of recriticality in the event of core damage.
3. Minimization of fuel vaporization and energetics in the event of recriticality.

4. Minimization of activity release outside the PCRV, given fuel vaporization.
5. Minimization of fission product and/or fuel vapor dose outside the containment building.

3.4.3.1.1. Level 1

Flooding the core with boronated water has been proposed as a "last-ditch" means of RHR to prevent cladding melting in a depressurized GCFR. Flooding would begin after all forced circulation loops (three main loops, three shutdown cooling system loops, and three CACS loops) fail by a common mode. Natural circulation would not be available at low helium pressures. The flooding concept could provide last-ditch cooling for refueling outages, maintenance requiring PCRV depressurization, and slow uncontrolled depressurization accidents.

Criticality calculations have been performed for a flooded GCFR core, and the results show that (1) an ~11% boric acid solution is sufficient to maintain a 1% shutdown margin, even without control rods, and (2) steam ingress before complete quenching does not overcome the shutdown margin with all control and shutdown rods inserted. The solubility of boric acid in water increases with temperature, so that above 53°C, an 11% solution is obtainable. Within the core and blankets, the water is initially at its saturation temperature. As steam is produced, the PCRV pressure increases somewhat, although it is not expected to exceed 4 MPa. Ultimately, a natural circulation path within a water-flooded PCRV would be established for long-term heat removal.

Although the spray and flooding systems have not been designed, a thermohydraulic scoping study has been performed which concludes that the minimum required quench front speed is obtainable with reasonable spray rates (25 to 36 liters/min per fuel assembly). Furthermore, the combination of spray and flooding systems can maintain cladding temperatures below that for significant metal/water reaction rates. It appears that the

combination of spraying and flooding for last-ditch prevention of cladding melting is feasible in principle. However, design aspects such as uniform introduction of sufficient water to each assembly, system configuration, and isolation during normal operation have not yet been adequately addressed.

3.4.3.1.2. Level 2

The core is expected to heat up if adequate cooling is not available for a sufficiently long time. The cladding and duct walls would melt, and the fuel would melt and slump. Unless sufficient fuel can be removed from the core or sufficient neutron poison added to the core in a timely manner, the material relocation reactivity insertion would overcome the shutdown margin. It is the function of the level 2 approach to investigate the ability of the fuel rod, fuel assembly, control assembly, and core-wide concepts to prevent recriticality.

One concept is to design each fuel assembly such that sufficient neutron absorber material can be introduced into the slumping fuel to prevent recriticality. Although a design has not been developed, certain fundamental questions must be answered to determine the feasibility of any design:

1. What is the best neutron absorber material for this purpose, and how much is required for the range of slumped core configurations?
2. What is the required timing for poison introduction, solution, and mixing into slumped fuel? Can this time constraint be achieved?
3. Can a concept be designed with acceptable normal operation performance penalties?

Selection of the poison material should include the following factors:

1. Solubility and speed of dissolution.
2. Solubility when the fuel freezes.
3. Melting point.
4. Stability against decomposition.
5. Neutron absorption effectiveness.
6. Density.
7. Form of stored poison (pellets, beads, balls, powder, or liquid).
8. Dimensional stability in neutron flux field.

A series of two-dimensional diffusion theory calculations was performed to determine the amount of poison which would be required to maintain a completely molten core subcritical. A tentative choice of europia as the poison material was made to facilitate the calculations. All cladding, duct walls, and fuel and control rods were presumed to have melted, all control and shutdown rods were postulated to have decomposed, and all B-10 was presumed to have left the core. A molten fuel layer rested on top of a steel-plugged lower axial blanket, and channels were plugged by solidified cladding. Molten duct wall steel formed the steel pool which overlaid the molten fuel layer. More than 20% by volume of the molten fuel pool must be europia. This is equal to over 2 metric tonnes of europia for the demonstration plant. In terms of the essential reaction constituents, the poison to fuel mixture was ~ 1.17 kg of europium metal per kilogram of fissile plutonium metal. This amount of europia could be accommodated in one tube per fuel assembly. Each tube would be 2.2 m long with a cross sectional area equivalent to that produced when the central 37 rods of the fuel assembly are removed.

3.4.3.2. SMART Program Objectives. The LOSC accident is important in establishing the GCFR level of safety. Although the GCFR program can use safety information for unprotected accidents from the LMFBR program, there is little corresponding information for the early phases of an LOSC accident in the GCFR. Hence, a GCFR analytical and experimental effort in this area is justified.

Since the change to an upflow, bottom-supported GCFR, the emphasis with respect to LOSC accident safety analyses has changed from early accident termination to accident mitigation. The primary objective of accident mitigation in the core is to prevent recriticality. The form and success of drainage or poison mitigation concepts will depend on the steel geometry through the early stages of fuel relocation. Furthermore, relocation of steel is itself an important reactivity feedback mechanism and also forms the framework for fuel relocation, which will influence the conditions under which criticality can occur.

FLS-1 and FLS-2 showed that natural convection within an assembly is an important factor in the assembly heat-up and steel relocation sequence of events. FLS-2 showed steel blockages well within the core even after 10 min of heat-up. Hence, natural convection may be an important factor in determining steel geometry. Simple models appear inadequate to predict this behavior. Indeed, the complexity of the coupled steel melting, relocation, and natural convection effects preclude a solely theoretical investigation.

The SMART program for the upflow core is required at least as much as the duct melting and fallaway test (DMFT) program was for the downflow core because of the increased concern over criticality and the increased difficulty in demonstrating accident mitigation with an upflow core. The SMART program is necessary for developing and confirming methods for investigating LOSC accident mitigation concepts and accident consequences as well as providing information which is directly applicable to mitigation potential.

Characterization of steel blockages and cladding and duct wall melting sequences in the presence of natural convection should be the primary experimental results to be sought. Blockage characterization is directly applicable to mitigation potential and determines criticality conditions.

The SMART facility at LASL appears to be the best vehicle for pre-molten fuel experiments to meet the objectives of accident mitigation, consequence determination, and methods development and confirmation. The technical basis for the SMART program is correlated with the LOSC accident safety objectives: evaluation of accident mitigation potential and concepts and determination of accident consequences. Development and confirmation of accident analysis methods is a necessary step toward fulfilling these objectives. Hence, the SMART effort should produce results in areas which are either not amenable to analysis or require experimental information to enable analysis. Figure 3-14 summarizes the six preferred areas of experimental results and the four way in which these results will be utilized in the LOSC accident evaluations.

The steel blockage formed by the relocated cladding and duct wall should be characterized by determining its location in the core, thickness, porosity or permeability, shape, and the completeness with which it blocks all coolant channels. This information can be directly used to determine the potential for molten fuel to melt through, drain through, or flow around the blockage. The cladding and duct wall melting sequences are also important in assessing the feasibility of the drainage concept and are the key items for method confirmation. Natural convection affects cladding and duct wall melting sequences and blockage characteristics. Hence, knowledge of the relevant flow loops and flow rates is desirable. Emphasis should be placed on providing a flow simulation which can be directly applied to a GCFR assembly. Steel blockage characterization, cladding and duct wall melting sequence, and natural convection flow rates are considered the most important results of SMART because of their direct applicability to the LOSC accident sequence of events. The guarded core module will be necessary to resolve these three items.

The other three experimental results in Fig. 3-14 are phenomenological weaknesses in the steel relocation analysis techniques. The uncertainty in theoretically defining molten steel flow rates is large because of the following: (1) the inadequate viscosity data for steel between the solidus and liquidus; (2) the lack of a criterion for initiation and cessation

Fig. 3-14. Scope of SMART

of steel motion; and (3) the lack of understanding of the mode of steel motion, especially in a natural convection environment (i.e., film flow, plug flow, droplet flow, etc.). A predictive capability for steel relocation is not possible without resolution of these items. The full-length subgroup facility may be adequate to resolve these phenomenological uncertainties. Bundle effects are phenomena, effects, or results which cannot be treated by conventional one- or two-dimensional single-channel analyses. Comparison of the theoretical models and the experimental results would determine the modeling corrections necessary to account for bundle effects. Because of radial heat losses in the full-length subgroup, the guarded core module will be required to study bundle effects.

The SMART program is currently conceived to have at least three tests in the guarded core module. The first, SMART-1, will be at GCFR system pressure with a preheating phase to establish a typical axial temperature profile followed by a heat-up phase to nearly complete steel melting. The test should have an inlet and outlet plenum to establish natural convection effects as typical of the GCFR as possible. The purpose of the test will be to characterize steel relocation and blockage formation, cladding and duct wall melting sequences, and natural convection. SMART-2 will be the same as SMART-1 to demonstrate the repeatability of the results. It will optimize instrumentation to resolve the data ambiguities of SMART-1, will answer questions raised by SMART-1, and will be a confirmation test of the predictive capability developed using SMART-1.

SMART-3 will investigate low-pressure, low-decay-heat steel melting and relocation sequences characteristic of a depressurized refueling mode in the GCFR. These results will be compared with those of SMART-2 to show the influence of natural convection on steel. This test follows the more complex high-pressure tests because the largest uncertainties and lack of understanding are related to the pressurized condition with natural convection. If the methods can adequately predict SMART-2, then a prediction of SMART-3 should be relatively straightforward. Tests beyond SMART-3 will depend on the results of the previous tests and the success of the method development effort. One objective of SMART-4 tests might be to investigate the effect of asymmetric heating rates.

3.4.3.3. Transient SCORIA/COBRA Method Development. The first use of the SCORIA/COBRA transient code package (Refs. 3-12 through 3-14) was made for experimental analysis. During the analysis, it became clear that the planned coupling of SCORIA and COBRA was inadequate. Hence, a more sophisticated coupling was developed and used for the FLS-2 analysis reported in Section 3.4.3.4.

SCORIA uses the COBRA calculations for the effects of helium heat transport. COBRA is limited to calculating helium temperatures and energy fluxes into and out of the helium, given SCORIA-supplied rod and wall temperatures. SCORIA is limited to calculating rod and wall (and insulation) temperatures, given COBRA-supplied fluxes into and out of the helium. Thus, COBRA is responsible for conservation of energy, mass, and momentum in helium, and SCORIA is responsible for overall conservation of energy including the helium effects determined by COBRA.

The SCORIA/COBRA theory can better be presented in the context of a specific problem. Figure 3-15 shows the analytical model for the FLS experiments. The experiment was designed to be azimuthally symmetric, so only the shaded 60-deg section of the bundle (Fig. 3-15a) was modeled. Since SCORIA does not have azimuthal capabilities, each rod and channel was azimuthally averaged into single rods and channels, as shown in Fig. 3-15b. The bundle was axially divided into seven upper axial blanket segments, thirteen core segments, and two lower axial blanket segments. The two lower axial blanket segments extend only one-third of the lower axial blanket length because natural convection was not expected (nor observed) in that region.

In the radial direction, SCORIA was set up to model the duct wall and insulation through the water-jacketed pressure vessel as an axially uniform structure. The insulation model consists of heat transfer nodes in every layer of the many-layered insulation. Within the duct, SCORIA models four electrical rod heaters as axially tapered graphite regions in the core and

Fig. 3-15. SCORIA model for FLS-2: (a) top; (b) side. Each layer of insulation and each heater rod internal component are allocated at least one radial node. Flow vectors shown are typical at axial midplane

molybdenum regions in the axial blanket. These four heaters are the equivalent of 1, 6, 12, or 18 rods in the appropriate hexagonal rings. The graphite taper yields the desired axial power distribution and the actual rod taper geometry is fed into a SCORIA electrical model. The electrical model accounts for the temperature dependence of electrical resistivity such that the current supplied at the common upper and lower electrodes is divided according to the electrical resistance of each hex ring. Each heater rod is located within an alumina electrical insulator and a nominal dimension GCFR stainless steel cladding tube.

The COBRA model differs from SCORIA in the radial dimension. COBRA models only the four numbered gas channels. Each channel has a "left" and "right" mean temperature for 22 axial segments (i.e., 7 upper axial blanket, 13 core, 2 lower axial blanket). The COBRA computation time for this model is large, and therefore an approach was required whereby COBRA would not have to perform transient calculations of the experiment. The approach assumed that the gas circulation in the FLS-2 experiment is nearly steady state at every instant. Such an assumption is reasonable in FLS-2 because the helium mechanical (i.e. acceleration) and thermal inertia is small.

SCORIA and COBRA were coupled such that SCORIA would predict (estimate) metal temperatures at time $t' = t + \Delta t$. COBRA would calculate the steady-state helium conditions for those metal temperatures. Since nothing is known a priori with respect to the natural convection helium flow distribution within the test bundle, it is necessary to employ the fully implicit solution capability of the transient version for the COBRA code. This capability was not previously available in the GCFR program, but was successfully adapted from the liquid coolant version of the COBRA transient code. Using the energy fluxes into the gas calculated by COBRA, SCORIA repeats the integration from t to t' , and then either predicts the next integration step or recalculates the SCORIA prediction for t' for a COBRA recalculation. The minimum iteration time interval Δt was expected to be nearly 30 s, because the transient is relatively slow. Unfortunately, the Δt required for a stable solution tends to decrease with time until Δt values are comparable to the COBRA time required to reach a steady state. Thus, the long COBRA computation time became limiting.

Therefore, a new coupling was devised which did not depend on COBRA reaching a steady state. The new coupling used a time scaling factor such that one second of COBRA time was equivalent to several seconds of experiment time. The requirements for this approach are identical to those for the steady-state approach. The new coupling is a Euler integration predictor-corrector. SCORIA first predicts time-varying cladding and duct wall temperatures by extrapolation from previous COBRA results and new electrical power input. COBRA is then executed over the same time period using cladding and duct wall temperatures from SCORIA as time-varying input. COBRA calculates the entire gas response and then supplies SCORIA with the time-varying algebraic energy (i.e., watts) into each gas channel "side." SCORIA then uses the COBRA-calculated gas response in a correction step and extrapolates to new temperatures for the next step. The predictor-corrector approach was successful, as demonstrated in the FLS-2 experiment analysis described in Section 3.4.3.4.

3.4.3.4. FLS-2 Experiment and Analysis. The FLS-2 experiment was completed at LASL. The experiment was formally intended as a heater rod development test, and as such, was an unqualified success. Unfortunately, the postmelting phase of the experiment was marred by metallurgical interaction with the FLS-2 molybdenum insulation. Thus, the postmelting results are difficult to evaluate. The secondary purpose of this experiment, i.e., providing instrumentation to support analytical method development, was also successful. Both the instrumentation and the posttest examination indicate that natural convection significantly influences the test results.

The FLS-2 experimental power input was $4.6\% \{[1 - \exp(-\text{time}/24) \text{ s}]\}$. The maximum power level of 4.6% of normal full GCFR power was chosen for consistency with the maximum power level which will be required in later SMART tests. Cladding melting began just after 260 s. Because coupling the cladding melting and refreezing process with the convecting helium effects requires substantial theoretical, analytical, and experimental development, this discussion is limited to the first 2 min of the FLS-2 transient.

Figures 3-16 and 3-17 compare the temperatures measured at the mid-upper axial blanket center rod and duct wall with the SCORIA/COBRA calculation (including convection) and the no-convection SCORIA calculations. Comparison of the latter curve with the measurements clearly shows the pronounced effect of natural convection heat transport in a high-pressure helium environment. Because thermocouples TBD1, TBD2, and TBD3 (Fig. 3-17) were symmetrically placed on midflats 120-deg apart, it is apparent that an asymmetric hot streak occurred near TBD2. The cause of the streak and its extent are uncertain, so no analytical evaluation was pursued.

The energy deposited in the upper axial blanket must come from the core. Therefore, relative to a no convection case, convection should reduce the temperature of the core midplane. Figures 3-18 and 3-19 show the temperature at the core midplane corresponding to the rod and duct wall locations of Figs. 3-16 and 3-17. Apparently, the hot streak (TBD2) does not extend this far into the bundle. Note that convection removes heat from both the midplane center and the midplane duct wall.

The ten thermocouple records discussed above and the eleven other thermocouple records distributed over seven axial locations are in agreement with the natural convection streamlines that can be inferred from COBRA. The streamlines at 120 s illustrated in Fig. 3-20 have a number of a priori predictable results: (1) flow penetrates the lower axial blanket owing to its downward momentum; (2) flow is upward along the entire axial length of channel 1; and (3) flow is downward along the entire duct wall. Note that the duct remains cool because of its thermal inertia and the heat transfer through the layered insulation to the water jacket heat sink at 20°C. Figure 3-20 also shows a number of theoretically consistent, but a priori unpredictable, results; e.g., there is very little cross flow in the core region. Furthermore, COBRA calculates very little turbulent momentum and energy transfer between channels in this region, and therefore the core flows are substantially channel flows. This reduces the uncertainty in the use of heat transfer and friction factor correlations that are developed predominantly for channel flow. The flow reversal at the upper boundary

Fig. 3-16. Comparison of FLS-2 experimental data and analytical predictions for central rod at upper axial blanket midplane

Fig. 3-17. Comparison of FLS-2 experimental data with analytical predictions for duct wall at UAB midplane

Fig. 3-18. Comparison of FLS-2 experimental data and analytical predictions for central rod at core midplane

Fig. 3-19. Comparison of FLS-2 experimental data and analytical predictions for duct wall at core midplane

Fig. 3-20. Streamlines inferred from SCORIA/COBRA analytical predictions for 120 s

forces the upward cooling flow in the core region of channel 3 sideways into channel 2 in the upper axial blanket. The upflow in channels 1 and 2 does not turn until the flow encounters the upper boundary.

Figure 3-20, along with additional COBRA diagnostic output, is qualitatively consistent everywhere. The SCORIA calculations are also qualitatively consistent everywhere. Therefore, one might consider that the slowly diverging bundle overtemperature (i.e., SCORIA/COBRA calculation compared with experiment) may be due to other effects. Several such areas can be easily identified. They were not previously evaluated because prior non-convecting analyses of the earlier FLS-1 experiment were grossly in error. Current studies are examining the effects of a slightly different insulation pattern in the upper axial blanket: the effect of a copper electrode at the bundle top that is in partial contact with the water-jacket-cooled electrodes; the effect of the general loss of helium inventory in both the experiment and the analysis; and various other experimental hardware-related effects. The sum of these effects may be sufficient to account for the slowly increasing temperature overprediction.

3.4.4. Primary Containment Technology

3.4.4.1. Postaccident Fuel Containment.

3.4.4.4.1. Analysis of Molten Fuel Containment in an Upflow Core

Detailed PAFC analysis for the upflow GCFR demonstration plant design was completed. This included behavior of the molten debris pool and distribution of decay heat removal. Figures 3-21 and 3-22 show the configuration used for these analyses. All fuel, blanket, shielding, and grid plate material from the core centerline to the radial blanket outer radius were assumed to be relocated and settled according to density into the lower cavity region, as shown in Fig. 3-22. Fuel and steel from the core was assumed to be molten initially, and all other materials were initially solid. All steel in the lower cavity inside the MgO layer was added to the

Fig. 3-21. Model of upper reactor cavity for PAFC analysis

Fig. 3-22. Model of lower reactor cavity and initial condition for PAFC analysis

steel region above the fuel and blanket layer, and the initial heat generation rate in the debris pool was assumed to be that corresponding to the decay heat after scram.

Figure 3-23 shows the melting and refreezing of the fuel and steel debris following a postulated core meltdown accident. The fuel and steel pools started without a lower crust. The lower fuel crust developed rapidly and later exceeded the upper and sideward fuel crusts because in the fuel pool, the convection-dominated heat transfer model always predicted a smaller fraction of the downward heat removal. However, in recent large-cell UO_2 pool tests (Ref. 3-15), the downward heat removal was found to exceed the upward heat removal in an electrically heated UO_2 pool. These test data contradict all previous water pool tests. Current results using Mayinger's convection model may predict a higher fraction of upward heat removal, so that reactor internals may reach a higher than actual maximum temperature. Since the purpose of this analysis was to determine whether the upward flowing heat can be removed by cavity liner cooling without helium convection, the use of Mayinger's model is conservative. The fuel pool reached a maximum size of 6 m^3 (65% of the fuel plus blanket materials) in 15 h. A lower steel crust did not develop in the 100-h time span of the analysis. From 20 to 53 hours, the steel region was completely molten, the sideward crust grew again, and cooling of the steel region began.

Figure 3-24 shows the temperatures in the fuel and steel debris. The fuel pool temperature is well below the boiling point and continuously decreases. Except for the high initial condition, the steel pool temperature is below 1500°C most of the time. The fuel-magnesia and steel-magnesia interface temperatures are always below 1500°C most of the time. The fuel-magnesia and steel-magnesia interface temperatures are always below 1500°C , which is moderate, so that the magnesia can serve as a permanent crucible.

Fig. 3-23. Crust thickness and pool volume for fuel and steel

Fig. 3-24. Temperature history of debris mass

Figure 3-25 shows the distribution of directional heat removal from fuel and steel debris. In the fuel region, upward heat removal is greater than 50% for up to 100 h, and sideward heat removal is insignificant. For the combined core debris of fuel and steel, however, sideward heat removal becomes dominant. Upward heat removal reaches only 25% and decreases below 20% after 40 h. The large fraction of upward heat removal from the fuel pool is redirected to the side wall of the steel pool; therefore, the combined upward heat removal is greatly reduced.

Figure 3-26 shows the heat balance. In the lower cavity, a quasi-steady state is reached 28 h after the accident; in the upper cavity, 65 h is required to reach the quasi-steady state. These curves also show that less than 1 MW is required (curve B) to remove the upward-flowing heat through the cavity liner, and less than 5 MW is required (curve F) to remove the total decay heat. The cavity liner cooling capacity is 2.5 MW during normal operation. Therefore, only a two-fold increase of the liner cooling heat exchanger capacity will satisfy the PAFC cooling requirement, although, at the cavity liner level, a local twenty-fold increase of cooling is required.

3.4.4.1.2. Comparison of Molten Fuel Containment Concepts.

Postaccident fuel containment concepts for GCFRs have been developed in Germany (Ref. 3-16) and the U. S. (Ref. 3-17). Among the many concepts, the ceramic crucible, the borax bath, the uranium metal bath, and the steel bath concepts have been studied. The analytical methods used for the evaluation of alternate concepts include Baker's (Ref. 3-18) empirical model for two-dimensional heat transfer in internally heated pools, conduction heat transfer from the side and bottom structures, and convective and radiative heat transfer from the pool surface to the PCRV internal structures.

The ceramic crucible utilizes a buildup of refractory materials which forms a crucible inside the liner to contain the molten core debris without melting or chemical attack of the crucible surface and at the same time to provide the required shielding for normal operation. Previous analyses

Fig. 3-25. Distribution of directional heat removal from debris mass

Fig. 3-26. Heat balance in central reactor cavity

(Ref. 3-17) for a 300-MW(e) GCFR show that this concept can be applied to the current GCFR design with minor modifications. The thick crucible wall provides a stored heat capacity that can last 30 h after core meltdown. The peak flux which eventually reaches the cavity liner is sufficiently low so that only a moderately enhanced liner cooling capacity will remove the entire downward-flowing heat. However, because of this thick crucible wall, the debris pool temperature reaches 3000°C, and the margin for fuel boiling under depressurized conditions is small. In addition, most of the core debris decay heat is driven upward, which makes this concept highly dependent on upward heat removal. Design provisions would be required to avoid the flotation of crucible blocks in the fuel pool.

The BORAX bath concept for the GCFR was proposed in Ref. 3-19. Steel boxes filled with borax ($\text{Na}_2\text{B}_4\text{O}_7$) are installed in the lower reactor cavity. Following a core meltdown, the oxide fuel is expected to dissolve in the liquid borax to form a compound solution pool. The dissolving process is controlled by steel box melting, so that the liquid borax is already at the melting point of steel, where a fast dissolving rate may be achieved. The low boiling point of borax (1700°C) may cause a borax vapor blanket to form at the fuel-borax interface, so that the fuel and steel may sink through the borax bed without dissolving the fuel. In addition, the borax pool may become separated from the fuel by an intermediate steel layer, interrupting the dissolving process. Small-scale simulation tests (Ref. 3-19) indicate that UO_2 dissolution can be accomplished in the presence of steel; larger experiments are currently in progress. Only 20% to 30% of the decay heat flows upward because of the low pool temperature. Sidewall and downward heat fluxes are increased, but the peak heat flux does not occur until about 10 h.

The heavy metal bath concept uses a large mass of high-density low-melting-point uranium metal alloy installed inside the lower reactor cavity. Following a core meltdown, a low-temperature pool of uranium alloy is expected to form, with solid fuel fragments in suspension. The molten pool is contained by the unmelted solid edge of the heavy metal. The principal advantage of this concept is its self-sealing feature. Air gaps between

structural alloy blocks become filled by the uranium alloy, which has a higher density than UO_2 , thereby preventing penetration of molten UO_2 into structural gaps and cracks, which can locally increase the heat flux to the cavity liner. A heat transfer study (Ref. 3-20) for the 1500-MW(e) GCFR shows that heat removal from the heavy metal bath is feasible with a wide range of suitable pool temperatures. The disadvantages of this concept include the high cost of uranium materials, the potential for metal-water reactions if the liner is breached, and the possibility of crusting on top of the heavy metal, which could suspend a significant fraction of the UO_2 above the pool. Uranium alloys also have a low heat capacity, requiring a 2-m-thick layer for a 4-h heat capacity.

The steel bath concept employs a large mass of stainless steel plates which melt following a core meltdown to form a "light metal bath." This concept is similar to the uranium bath except that the core debris is heavier than the pool material and is collected at the bottom of the steel pool. A refractory layer placed between the steel and the cavity liner is thus needed to protect the liner from potential hot spot effects. Analysis of steel bath heat transfer (Ref. 3-21) shows that a steel core retention system has a greater stored heat effect than the uranium system, and therefore the liner heat flux and temperatures are lower. Similar to the ceramic crucible concept, this concept can be accommodated without a large cost of significant design changes.

Table 3-11 compares the four concepts and lists the important parameters. The ceramic crucible is the simplest concept, but it is highly dependent on upward heat removal; whereas the borax bath and uranium bath concepts offer better performance but require major design changes and experimental development. The steel bath concept appears to be an interesting compromise. A combination of the essential features of two concepts, i.e., a heavy metal base with an overlaying steel bath, may offer further improvements.

TABLE 3-11
COMPARISON OF MOLTEN FUEL CONTAINMENT CONCEPTS

Parameter	Ceramic Crucible	Borax Bath	Uranium Bath	Steel Bath
Pool temperature (°C)	High (>3000)	Low (1427)	Low (>1200)	Medium (>1500)
Cavity liner temperature (°C)	Low (150-200)	High (300-400)	High (280-350)	Medium (250-300)
Time of maximum liner heat flux (hr)	Long (20-40)	Medium (6-10)	Short (3-4)	Medium (6-10)
Maximum liner heat flux (kW/m ²)	Low (50-100)	High (200-300)	High (200-300)	Medium (150-250)
Fraction of upward heat removal	High (0.6-0.8)	Low (0.2-0.3)	Medium (0.3-0.4)	Low (0.1-0.3)
Central cavity height required	Small	Large	Large	Large
Design changes needed	Minor	Major	Major	Minor
Need for experimental work	Low	High	High	Medium
Recriticality potential	Low	Low	Future work	Future work
Pool manageability	Medium	Low	High	High
Fuel penetration and material flotation	Yes	Yes	No	Yes
Scaleability	High	Low	Medium	Medium
Cost	Low	Medium	High	Low
Problems in normal reactor operation	No	Major	Minor	No

3.4.4.2. Containment Mitigation Option Study. The containment mitigation option study investigates two levels of accident consequence reduction: the primary containment (PCRv) and the secondary containment. A preliminary study has been completed with satisfactory results, and a possible design option was found for the primary containment. For the secondary containment, no radiological consequence mitigation is necessary beyond that provided for the design basis site suitability source term for CDA mitigation as long as containment integrity is maintained. It may, however, be necessary to consider containment integrity protection systems.

Four consequence-mitigating design options were initially considered for the PCRv: PCRv sprays, PCRv relief valve filters, PCRv relief valve blowdown into helium storage bottles, and a sealed PCRv without relief valves. These concepts were evaluated with respect to their ability to mitigate the release of fuel and fission product activity to the containment following a CDA.

The most likely release path to the containment is considered to be through the PCRv relief valve. PCRv spray systems for aerosol washout were eliminated because such a spray may not be able to prevent PCRv relief valve opening in the first place and secondly because the environment inside the PCRv is difficult to predict following a CDA. The sealed PCRv concept, i.e., without a pressure relief valve, was eliminated because of ASME code requirements and because the absence of a relief valve would force other leak paths such as penetration seals or circulator seals which may be less predictable. Furthermore without a relief valve, the possibility of missile generation would increase. Helium blowdown bottles connected to the PCRv relief valves are technically feasible but require a large space inside the containment. The decay heat load in the bottles is manageable if a reasonably uniform distribution between bottles can be assured. A filter connected to a relief valve would experience a very large heat load and would require a very large sand filter.

A more sophisticated containment air cleaning system which may be adaptable to the PCRV relief valve discharge has been reported (Ref. 3-22). The system incorporated a quench tank, a jet venturi scrubber, a high-efficiency fibrous scrubber, and a high-efficiency particulate absorber (HEPA) filter. A scoping analysis showed that the air cleaning system could be used between the PCRV and containment to reduce the source term. There appear to be no fundamental feasibility problems, but no equipment sizing for the GCFR has been performed.

It is recommended that this air cleaning system or the PCRV blowdown bottles be considered in more detail if mitigation of the containment source term becomes necessary in the future. However, the analyses performed on the secondary containment show that source term mitigation may not be necessary. The basic design suggested for the GCFR containment/confinement system appears adequate to mitigate environmental doses for extremely large CDA source terms as long as realistic dose assessment models are used rather than conservative NRC licensing assumptions.

A radiological consequence study was completed. This study had two objectives: (1) to determine the exclusion area boundary (EAB) and the low population zone (LPZ) distance for a 300-MW(e) GCFR demonstration plant at a typical site using the reference containment design, the GCFR site suitability source term (SSST), and NRC licensing assumptions and (2) to perform a parametric study with realistic consequence models of the radiological consequences of an arbitrary source term in the containment. It was desired to determine the source term size which would yield expected 10CFR100 dose consequences at the EAB and LPZ.

The secondary containment consisted of a steel containment building with a concrete confinement. The SSST assumed that 100% of the noble gases, 25% of the halogens, 1% of all other fission products, and 1% of the fuel inventories were released into the containment building. An arbitrary CDA source term was parametrically varied from the SSST to the ultimate limit of all of the core fuel and fission product available as aerosol in the containment. The TDAC computer code (Ref. 3-23) calculated the activity release and the doses.

Based on the SSST and conservative licensing assumptions, the EAB is 630 m and the LPZ distance 750 m. With realistic dose consequence models, 10CFR100 dose limits are reached at the EAB and the LPZ if a source term exceeding 65% of the core fuel inventory is postulated as an aerosol in the containment. Furthermore, if wind direction averaging is used, a source term in excess of 100% of the fuel inventory is required to reach 10CFR100 consequences at the low LPZ distance.

Bone doses dominate the accident consequences for all cases considered. Six fuel isotopes account for most of the dose to the bones, lungs, and whole body. Direct bypass leakage is the predominant activity release mechanism. The dose can be reduced by two orders of magnitude by eliminating bypass leakage.

Three conclusions were drawn from this analysis:

1. The conservatisms inherent in licensing assumptions are sufficient to compensate for an extremely large source term.
2. A GCFR containment can be designed to contain both the SSST and an extremely large source term with reasonable site boundaries. Additional design mitigation features can reduce the dose, but are not needed.
3. A better understanding of the containment failure modes is needed to determine the failure modes of the containment. The analysis assumed that the containment functioned as designed.

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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. These components include the main helium circulator, 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, or modification and check-out of the facility.

During this quarter, the R. M. Parsons Company completed the engineering phase of a conceptual design and cost estimate for a GCFR circulator test facility. This facility will use the existing HTGR circulator test facility. R. M. Parsons is also investigating a parallel GCFR circulator test facility design which uses the experimental organic critical reactor facility at INEL. In addition, the facility functional design criteria have been revised.

5.0 ALTERNATE FUEL CYCLES

5.1. ALTERNATE FUEL CYCLE STUDIES

The purpose of this task is to determine the characteristics of a GCFR fueled with a variety of fissile and fertile materials. These characteristics will be used to evaluate various reactor fuel strategies which could be employed to improve the proliferation resistance and/or energy capability of future nuclear power systems. GCFR mass flow information for the Nonproliferation Alternate Systems Assessment Program (NASAP) and International Nuclear Fuel Cycle Evaluation (INFCE) studies are also prepared and reviewed under this task.

The initial plan for the alternative fuel cycle core design study called for a preliminary analysis and evaluation phase to identify and characterize the cycle of interest. The second phase was to provide an initial optimization of only the attractive fuel cycles. The third phase was planned to develop a fuel-cycle-independent core design which would permit specification of design parameters important for the breeding performance for any of the fuel cycles of interest, e.g., geometric envelope, circulation power, steam generator size. Information available from this study could then be used to protect the noncore components against the uncertainty which continues to surround fuel cycles for advanced reactors. Table 5-1 presents an outline of the study as initially proposed. The first phase, completed in 1977, indicated that core fuel cycles other than Pu/U, Pu/Th, and LEU-233 were of little interest for further core design study.

TABLE 5-1
GCFR ALTERNATE FUEL CYCLE STUDY: CORE DESIGN

Phase I: preliminary analysis and evaluation

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

Phase II: core design and material optimization

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

Phase III: fuel-cycle-independent core design

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

5.1.1. Selection of Materials

Because the preliminary studies were intended to identify the cycles of interest for further study, no attempt was made to determine or use the most attractive core fuel or structural material. The initial studies assumed oxide fuel with nimonic PE-16 cladding and structure. Subsequent analysis has shown that the use of other materials could significantly improve core breeding performance. Because of the high nickel content of PE-16, breeding performance can be improved by using the current reference cladding, i.e., stainless steel 316.

The national fast breeder reactor cladding and alloy material program (Ref. 5-1) was consulted, and the characteristics of the six alloys selected for further study were reviewed. It appears that during the next decade, 316 stainless steel may be the only structural material having a data base adequate for licensing. However, any of the advanced materials could be made available through a well directed qualification program. In the longer term, a structural material with lower neutron-induced swelling, lower neutron absorption, and adequate high-temperature strength will be required. Currently available information indicates that the ferritic alloys, and particularly HT-9, possess the desired characteristics. Hence, HT-9 has been selected as the fuel claddings and structural material for the advanced commercial design studies; 316 stainless steel continues to be used for demonstration and first commercial cladding.

The selection of the fuel form for advanced systems is relatively straightforward. In terms of breeding performance, metals can be superior to carbides, which can be superior to oxides. Presently, only the oxide fuel has been sufficiently characterized for licensing. Therefore, the advanced commercial design studies are done for both oxide and carbide fuels.

5.1.2. Core Design and Analysis

5.1.2.1. Fuel-Cycle-Independent Core Designs. For each GCFR reactor system considered, a fuel-cycle-independent core design was determined, including core dimensions, core primary pressure drop, and circulator power. Optimization was based upon a Pu/U-238 fueled core. Table 5-2 lists the key fuel-cycle-independent core design parameters obtained for each of the following systems from the CALIOP code (Ref. 5-2):

1. Two-loop demonstration plant.
2. First commercial GCFR.
3. Advanced oxide GCFR.
4. Advanced carbide GCFR.

The two-loop demonstration plant and the first commercial GCFR have almost identical core configurations and system parameters. Each coolant loop (six in the commercial plant) provides 200 MW(e) of power and operates at a primary pressure of 10 MPa. Low-power helium circulators (22,000 hp, nominal) reduce the technological risk associated with the early plants. The core mechanical dimensions and temperatures are nearly identical, further reducing the risk for initial commercialization. The fuel cladding and assembly duct material is 316 stainless steel.

The advanced oxide and advanced carbide fuel-cycle-independent core designs [both 1200 MW(e)] were developed based on increased primary pressure (12 MPa) and circulator power (45,000 hp, nominal) and utilization of the advanced alloy HT-9 for the cladding and duct material. The increase in pressure and circulator power results in a compact fuel rod lattice and high fuel volume fractions of about 0.35 and 0.32 in the advanced oxide and carbide designs, respectively. The amount of structure in the advanced cores is minimized by using thin (3 mm) fuel assembly ducts and increasing the separation of fuel rod spacers to 300 mm.

TABLE 5-2
SUMMARY OF CORE DESIGN PARAMETERS

	Two-Loop Demonstration Plant	First Commercial Plant	Advanced Oxide	Advanced Carbide
Reactor core				
Reactor power MW(t)[MW(e)]	1076(400)	3600(1200)	3600(1200)	3600(1200)
Primary system pressure (MPa)	10	10	12	12
Peak linear rating (kW/m)	2.96	3.26	4.57	6.10
Core inlet temperature (°C)	302	311	308	307
Core outlet temperature (°C)	547	564	562	534
Number of rows in radial blanket	3	3	3	3
Axial blanket (mm)	600	600	600	600
Number of fuel assemblies	150	450	378	312
Number of control assemblies (dedicated)	19	19	19	19
Number of radial blanket assemblies	156	252	234	216
Number of core enrichment zones	3	3	3	3
Core height (mm)	121	121	106.06	96.35
Core diameter (cylinder) (mm)	1407.11	2312.72	1933.97	1846.74
Core P (MPa)	0.16	0.18	0.44	0.44
Fuel cycle length (full-power days)	381.0	364.27	250.96	231.09
Fuel Assembly				
Number of rods per assembly	264	264	271	271
Assembly pitch (mm)	206.16	203.40	184.87	193.33
Number of support rods per assembly	7	7	0	0
Duct thickness (mm)	4.05	3.10	3.00	3.00
Interassembly gap spacing	10.63	12.94	6.50	6.50
Rod spacer separation (mm)	200	200	300	300
Duct material	316 stainless steel	316 stainless steel	HT-9	HT-9
Volume fractions inside midassembly gap				
Fuel	0.27920	0.29977	0.37215	0.34120
Structure	0.17298	0.14584	0.14360	0.13443
Helium	0.54702	0.55439	0.48325	0.50965
Fuel rod				
Cladding material	316 stainless steel	316 stainless steel	HT-9	HT-9
Outside diameter (mm)	8	8	8	8
Cladding thickness (mm)	0.514	0.435	0.432	0.432
Pitch (mm)	11.19	11.12	10.31	10.80
Smear density (inside cladding)	0.8543	0.8587	0.9000	0.9000
Peak cladding temperature (°C)	750	750	750	750

5.1.2.2. Analytical Methods and Models. Figure 5-1 shows the calculational sequence used for the core design analyses. Core geometries and fuel rod and assembly dimensions were obtained from using CALIOP, a code which couples a multichannel thermohydraulic model and a one-group diffusion-depletion nuclear model. Major system parameters such as primary system pressure, helium circulator power, maximum linear heat rate cladding temperature, and fuel and structural materials are constraints for the CALIOP calculations. CALIOP was also used to determine the enrichment zoning patterns to minimize compound system doubling time (CSDT).

Given the general core configuration from CALIOP, an iterative sequence of calculations was carried out using three computer codes: (1) FEVER (Ref. 5-3), (2) 2DB (Ref. 5-4), and (3) PERT (Ref. 5-5). The FEVER code was used to perform a one-dimensional (radial geometry) diffusion-depletion calculation through the equilibrium cycle, including a fuel feed enrichment search. The FEVER model shown in Fig. 5-2 was used for all cases analyzed. Each spatial region of FEVER contains subregions which refer to temporal divisions for discharging, reloading, or shuffling specified fractions of the fuel and radial blanket assemblies in each region at specified intervals.

The 2DB code performed two-dimensional (R-Z geometry) diffusion-depletion calculations for the equilibrium cycle using BOEC atom densities obtained from FEVER. Figure 5-3 shows the R-Z model used for the 2DB analysis of each case. The spatial region boundaries in the 2DB model correspond to those used in the FEVER model.

The PERT code used the R-Z fluxes from the 2DB analysis to compute the axial buckling and axial blanket flux disadvantage factors to be used in the succeeding FEVER calculation, completing an iterative cycle. The axial blanket flux disadvantage factors are volume-averaged ratios of fluxes in the axial blanket to fluxes in the corresponding core region. They were used by FEVER to calculate the appropriate flux for depleting the axial blanket subregions. The 2DB results enabled accurate calculation of the

Fig. 5-1. Analytical procedure

* REGION NUMBER

** SUBREGION NUMBER

Fig. 5-2. Region and subregion diagram for radial portion used for FEVER calculations

Fig. 5-3. Region diagram for R-Z geometry model used for 2DB calculations

maximum liner heat rate in each core zone. FEVER relative zone enrichments were altered to obtain a more desirable radial power distribution. The iterative calculations were repeated until good agreement was obtained between 2DB and FEVER (e.g., nuclide weights and eigenvalues). The cross sections for all the calculations were derived from ENDF-IV data.

5.1.2.3. Fuel Management. As explained in Section 5.1.2.1, the core design for each reactor type is independent of the fuel cycle chosen, and all cores have an 8-mm fuel rod o.d. Moreover, the overall fuel management scheme is the same for each reactor system. The radial blanket management scheme includes semiannual assembly shuffling from row 3 to row 1 to row 2. The best shuffling pattern simultaneously minimizes limited maximum radial blanket rod linear heat rate (at the core - radial blanket interface) and radial shielding requirements.

The FEVER calculation incorporates the details of the core and radial blanket management scheme through 10 fuel cycles from BOL to end of equilibrium cycle (EOEC). The core, axial blanket, and radial blanket subregions are discharged, reloaded, or shuffled according to the sequence shown in Table 5-3. One segment (equal to one-third) of the core and axial blanket is reloaded at the end of each cycle, and each segment resides in the core for three cycles. The radial blanket assemblies reside in the core for 6 yr, except for 6 and 12 assemblies in radial blanket rows 2 and 3, respectively. Each year, half the row 1 assemblies are shuffled to row 2, half the row 2 assemblies are shuffled to row 1, and half the row 2 and row 3 assemblies are discharged and reloaded with the fuel assemblies. The 6 and 12 extra assemblies of rows 2 and 3, respectively, remain in the core for 10 yr before being replaced with fresh assemblies. Figures 5-4 through 5-7 show one-third core layouts, including enrichment zone boundaries and control assembly locations for each core analyzed.

The performance of the advanced oxide using the Pu/Th/Th/Th cycle is significantly below that for the other fuel cycles. The neutron production rate from fertile fission (most fission occurs in the core) is about a factor of 2.7 below that of the Pu/U/U/U cycle. The CSDT is approximately

TABLE 5-3
CORE AND RADIAL BLANKET MANAGEMENT SCHEME USED IN FEVER ANALYSIS

Time Point	Core Subregion Reloaded with Fresh Fuel	Subregion Discharged	Radial Blanket Subregion Shuffling	Subregion Reloaded with Fresh Fuel
BOIC	All	None	None	All
BOC2	1,7,13,4,10,16	None	None	None
BOC3	2,8,14,5,11,17	23,26	19 → 23, 21 → 26, 29 → 19, 32 → 21	29,32
BOC4	3,9,15,6,12,18	24,27	20 → 24, 22 → 27, 30 → 20, 33 → 22	30,33
BOC5	1,7,13,4,10,16	23,26	19 → 23, 21 → 26, 29 → 19, 32 → 21	29,32
BOC6	2,8,14,5,11,17	24,27	20 → 24, 22 → 27, 30 → 20, 33 → 22	30,33
BOC7	3,9,15,6,12,18	23,26	19 → 23, 21 → 26, 29 → 19, 32 → 21	29,32
BOC8	1,7,13,4,10,16	24,27	20 → 24, 22 → 27, 30 → 20, 33 → 22	30,33
BOC9	2,8,14,5,11,17	23,26	19 → 23, 21 → 26, 29 → 19, 32 → 21	29,32
BOEC	3,9,15,6,12,18	24,27,25,28,31,34	20 → 24, 22 → 27, 30 → 20, 33 → 22	30,33,25,28,31,34

Fig. 5-4. Core layout for 400-MW(e), two-loop demonstration plant (150 fuel assemblies, 3-row radial blanket, 19 dedicated control assemblies, 1/3 of core)

Fig. 5-5. Core layout for 1200-MW(e) first commercial GCFR (450 fuel assemblies, 3-row radial blanket, 19 dedicated control assemblies, 1/3 of core)

Fig. 5-6. Core layout for 1200-MW(e) advanced oxide GCFR (378 fuel assemblies, 3-row radial blanket, 19 dedicated control assemblies, 1/3 of core)

Fig. 5-7. Core layout for 1200-MW(e) advanced carbide GCFR (312 fuel assemblies, 3-row radial blanket, 19 dedicated control assemblies, 1/3 of core)

doubled to 17.0 yr. Although the growth potential of this system is limited, the annual production of 932 kg of U-233 (646 kg plutonium destroyed) offers the versatility of symbiotic systems and denatured fuel cycles. The advanced oxide design with Pu/U-238 cores have a high core conversion, resulting in small changes in K_{eff} over a fuel cycle (+\$0.30 with uranium blankets and -\$0.57 with thorium blankets). Even with the Pu/Th core, the K_{eff} decrease is only about \$2.

The performance of the advanced carbide systems is somewhat better than that of the advanced oxide systems for corresponding fuel cycles. The higher heavy metal density attainable with the carbide fuels results in a harder spectrum and larger neutron production terms. The carbide system neutron production terms are about 5% to 7% higher than those for the oxide system. Fertile fission and leakage is increased, and absorption in structure and fission products is reduced. The CSDT values are 6.2, 7.5, and 14.7 yr for the Pu/U/U/U, Pu/U/Th/Th, and Pu/Th/Th/Th cycles, respectively.

5.1.3. Results

Table 5-4 lists the fuel-cycle-independent core design/fuel cycle combinations which were studied, Table 5-5 summarizes the nuclear performance for each fuel-cycle-independent core design - fuel cycle combination, and Table 5-6 gives the inventory and annual mass flow requirements for advanced GCFRs. The two-loop demonstration plant operating on the Pu/U/U/U cycle has a breeding ratio of 1.29 and a CSDT of 28.4 yr. The low performance is due to the large finite inventory and low fuel volume fraction resulting from the open core lattice. Even with the same core configuration, the first commercial GCFR attains a CSDT of 16.0 and 17.9 yr for the Pu/U/U/U and Pu/U/Th/Th cycles, respectively. The first commercial doubling times could be lowered by about 1.5 yr by replacing the 316 stainless steel cladding and duct with HT-9 and increasing the primary system pressure by 1 MPa to 11 MPa.

TABLE 5-4
FUEL CYCLES AND CORE CONFIGURATIONS STUDIED

Core	Reactor System	Material Loaded				Material Bred			
		Core		Axial Blanket	Radial Blanket	Core		Axial Blanket	Radial Blanket
		Fissile	Fertile			Destroyed	Bred		
1	Two-loop demonstration plant	Pu	U-238	U-238	U-238	Pu	Pu	Pu	Pu
2	First commercial GCFR	Pu	U-238	U-238	U-238	Pu	Pu	Pu	Pu
3	First commercial GCFR	Pu	U-238	Th	Th	Pu	Pu	U-233	U-233
4	Advanced oxide GCFR	Pu	U-238	U-238	U-238	Pu	Pu	Pu	Pu
5	Advanced oxide GCFR	Pu	U-238	Th	Th	Pu	Pu	U-233	U-233
6	Advanced oxide GCFR	Pu	Th	Th	Th	Pu	U-233	U-233	U-233
7	Advanced carbide GCFR	Pu	U-238	U-238	U-238	Pu	Pu	Pu	Pu
8	Advanced carbide GCFR	Pu	U-238	Th	Th	Pu	Pu	U-233	U-233
9	Advanced carbide GCR	Pu	Th	Th	Th	Pu	U-233	U-233	U-233

TABLE 5-5
SUMMARY OF NUCLEAR PERFORMANCE FOR REACTOR SYSTEM FUEL CYCLE COMBINATIONS

	Case								
	1	2	3	4	5	6	7	8	9
Reactor	Demonstration	First commercial	First commercial	Advanced oxide	Advanced oxide	Advanced oxide	Advanced oxide	Advanced oxide	Advanced oxide
Core	Pu/U-238	Pu/U-238	Pu/U-238	Pu/U-238	Pu/U-238	Pu/U-238	Pu/U-238	Pu/U-238	Pu/Th
Axial blanket	U-238	U-238	Th	U-238	Th	Th	U-238	Th	Th
Radial blanket	U-238	U-238	Th	U-238	Th	Th	U-238	Th	Th
Fissile inventory(a) (kg)	2979.8	6073.8	6234.8	4284.6	4408.4	5216.6	3874.5	4020.3	4653.2
Cycle length (yr)	1.3919	1.3306	1.3306	0.9167	0.9167	0.9167	0.8442	0.8442	0.8442
Processing loss (kg/cycle)	19.7	49.2	50.3	35.9	36.6	40.8	33.7	34.0	37.2
Pu-241 decay loss (kg/cycle)	3.8	8.4	8.7	6.1	6.3	7.7	5.5	5.8	7.0
Fissile gain (kg/cycle)	129.2	495.7	460.8	489.3	458.2	314.4	549.7	479.7	302.5
Breeding ratio(b)	1.29	1.38	1.36	1.57	1.54	1.36	1.75	1.65	1.38
CSDT	28.4	16.0	17.9	8.30	9.20	17.0	6.19	7.46	14.7
K _{eff} /cycle (\$)	-7.51	-3.81	-4.50	+0.30	-0.57	-2.26	+2.13	+0.99	-4.87

$$(a) \quad CSDT = \frac{\ln 2 \times M (1 + RF \times T_{ex}/T_{cyc})}{(G - L_p - L_d) \times (1/T_{cyc})}$$

where CSDT = compound system doubling time (yr),

M = BOEC fissile inventory for core assemblies only (excluding blanket fissile) (kg),

RF = fraction of core replaced at refueling (1/3),

T_{ex} = external cycle time (assumed 1 yr) (yr),

T_{cyc} = cycle length between refuelings (75% capacity) (yr),

G = fissile gain per cycle (75% capacity) (kg/cycle),

L_p = reprocessing loss (2% of EOEC fissile discharge) (kg/cycle),

L_d = Pu-241 decay loss for external cycle (kg/cycle).

(b) Breeding ratio = fissile Pu production/fissile Pu absorption at BOEC (including Pu-241 decay and excluding U-235 destruction).

TABLE 5-6
GCFR ADVANCED OXIDE AND CARBIDE MASS FLOW REQUIREMENTS

	Case					
	4	5	6	7	8	9
Fuel type	Oxide	Oxide	Oxide	Carbide	Carbide	Carbide
Core	Pu/U-238	Pu/U-238	Pu/Th	Pu/U-238	Pu/U-238	Pu/Th
Axial blanket	U-238	Th	Th	U-238	Th	Th
Radial blanket	U-238	Th	Th	U-238	Th	Th
BOL inventory [kg/MW(e)]						
Th-232	0	85.10	109.07	0	88.95	109.53
U-233	0	0	0	0	0	0
U-235	0.240	0.054	0	0.262	0.055	0
U-238	121.1	27.46	0	140.3	28.04	0
Pu-239 + Pu-241	3.562	3.679	4.460	3.226	3.359	3.965
New yearly requirements(a) [kg/MW(e)-yr]						
Th-232	0	0.520	1.194	0	0.555	1.188
U-233	0	-0.467	-0.932	0	-0.494	-0.936
U-235	0.017	0.009	0	0.018	0.009	0
U-238	1.375	0.820	0	1.455	0.829	0
Pu-239 + Pu-241	-0.445	0.042	0.646	-0.543	0.020	0.637
Specific power (BOL) [MW(t)/kg-fissile]	0.84	0.82	0.67	0.93	0.89	0.76
Breeding ratio	1.57	1.54	1.36	1.75	1.65	1.38

(a)Based on equilibrium cycle and 0.75 capacity factor.

The benefits of the high fuel volume fraction attainable with increased circulator power and primary sytem pressure can be seen in the performance of the advanced systems. The Pu/U/U/U cycle is superior to all other fuel cycles for maximizing energy potential and deployment rate. This fuel cycle results in the lowest fissile inventory and CSDT and highest breeding ratio of the fuel cycles considered. For this cycle, the advanced oxide attains a CSDT of 8.3 yr and has a net annual production of 445 kg plutonium per GW(e).

Replacing the uranium fertile material in the blankets with thorium results in a modest decrease in nuclear performance, with CSDT increasing about 10% to 9.2 yr. The system is self-sustaining with plutonium and a significant U-233 producer. The resulting annual production of 467 kg of U-233 (42 kg of plutonium destroyed) per GW(e) can fuel advanced converter reactors (ACRs) in a symbiotic system or can be used in denatured U-233/U-238 cycles in any reactor. Table 5-7 shows the neutron balances for the advanced oxide and carbide systems. The slight decrease in breeding performance with thorium blankets is primarily from the 12% decrease in fast fission in the blankets.

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TABLE 5-7
NEUTRON BALANCES FOR ADVANCED OXIDE AND CARBIDE GCFRS

	Case					
	4	5	6	7	8	9
Fuel type	Oxide	Oxide	Oxide	Carbide	Carbide	Carbide
Core	Pu/U-238	Pu/U-238	Pu/Th	Pu/U-238	Pu/U-238	Pu/Th
Axial blanket	U-238	Th	Th	U-238	Th	Th
Radial blanket	U-238	Th	Th	U-238	Th	Th
Neutron production						
Fissile materials	2.169	2.470	2.488	2.555	2.543	2.553
Fertile materials	0.661	0.580	0.244	0.800	0.681	0.276
Total	3.130	3.050	2.732	3.355	3.224	2.829
Neutron losses						
Fissile absorption	1.000	1.000	1.000	1.000	1.000	1.000
Fertile fission	0.233	0.205	0.088	0.283	0.241	0.100
Fertile capture	1.610	1.576	1.396	1.786	1.688	1.414
Leakage	0.020	0.019	0.017	0.022	0.032	0.082
Carbon or oxygen absorption	0.056	0.053	0.050	0.058	0.074	0.076
Structural absorption	0.086	0.078	0.065	0.076	0.073	0.063
Fission products	0.044	0.042	0.031	0.040	0.038	0.029
Other absorption	0.081	0.077	0.085	0.090	0.078	0.115
Total	3.13	3.050	2.732	3.355	3.224	2.829