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ORNL-5119

Gas-Cooled Fast Reactor Program Progress Report for January 1, 1974, Through June 30, 1975

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ORNL-5119
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GAS-COOLED REACTOR PROGRAMS

**GAS-COOLED FAST REACTOR PROGRAM PROGRESS REPORT
FOR JANUARY 1, 1974, THROUGH JUNE 30, 1975**

Uri Gat, Program Manager

Paul R. Kasten, Program Director

JUNE 1976

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FOREWORD

The GCFR program at ORNL developed during the reporting period into an important and substantial part of the National effort; in the process, it has become a better structured and oriented program. In this development, ORNL personnel have participated actively in outlining sections of the National GCFR program plans. This report is the first separate GCFR Progress Report; previous progress reports were incorporated into overall GCFR Programs progress reports.

The major effort at ORNL is devoted to the *Core Flow Test Loop* (CFTL). This program is on the critical path of the GCFR schedule, with the CFTL providing information necessary for fuel element design and analysis verification. In particular, it will demonstrate the structural and thermohydraulic transient behavior of the fuel bundle and the rod cladding, and will contribute to safety analyses and design improvements of future GCFRs. Thus, the early completion of the test program is essential to early availability of the Gas-Cooled Fast Breeder, and this reactor could be an important contribution to the national energy supply. The CFTL schedule has benefited by initiation of gas circulator purchase late in FY 1975. The heater development also progressed in the reporting period. The CFTL test plan was developed through close cooperation with General Atomic Co. (GA). Also, significant information related to CFTL needs is derived from international programs.

The *GB-10* irradiation capsule is an ongoing program giving information on fission product release from vented fuel elements as well as fuel performance information. Improved apparatus for on-line Ge(Li) detector analysis was completed and used intensively in the report period. Gaseous fission product venting and the release fraction derived from the capsule were utilized in the Nationally used LIFE code. Tritium monitoring equipment was developed and installed in the capsule system. The flow restriction (conductance) measurements derived from the capsule are unique, and the capsule provides a major data source for correlating the performance of GCFR vented and pressure-equalized

fuel (with roughened cladding) to the performance of LMFBR fuel. The information derived contributes to both the GCFR and LMFBR programs.

The *PCRV-Closures* program concentrates on the experimental and analytical study of the closure plugs for PCRV cavities. As in many areas of the GCFR, strong international ties smooth the flow of information and improve the overall efficiency of the program. Efforts concentrate on the testing of a 1/15-scale model and plans for a 1/4-scale model are being prepared.

The *Shielding* work emphasizes shielding and streaming calculations. A benchmark streaming experiment at the Tower Shield Facility was performed and constitutes a major milestone in the confirmation of calculational methods. The shielding design analysis effort, aimed at optimization of the shield, can lead to significant cost reductions in the PCRV.

The *System Response* efforts concentrated on modeling of the steam generator, and the dynamic response of that component to transients was investigated. Shortage of funds necessitated discontinuation of this investigation.

All the GCFR programs are closely coordinated with GA and with the National and international efforts placed on the GCFR. Further, coordination exists with those parts of the LMFBR program that are related to the GCFR program.

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SUMMARY

1. GCFR PRESSURE VESSEL AND CLOSURE STUDIES

Planning for model tests of the prestressed concrete reactor vessel closures for the 300 MW(e) GCFR has been finalized and the planning document published. The tests will be conducted on 1/15-scale and 1/4-scale models of the steam generator cavity and central core cavity closure plugs. The 1/15-scale models will be used for preliminary scoping tests before proceeding with the more complex and costly 1/4-scale model tests.

Analytical studies of the steam generator cavity closure plug have been conducted and the model test plan prepared. Elastic axisymmetric analyses have been made of three candidate plug configurations using two finite element codes, CREEP-PLAST and STATIC SAP. The method of analysis is described together with the results of the analyses of the steam generator cavity closure. A mechanical properties "smearing" technique was used to obtain both satisfactory and economical axisymmetric analyses of nonaxisymmetric portions of the steam generator cavity model.

After several conceptual designs of the 1/15-scale steam generator cavity closure model were evaluated, the use of a ring of spherical balls in place of the long slender toggle hold-down members of the prototype was selected and the proposed test fixture analyzed. Loading tests of individual steel bearings in spherical seats were conducted to verify capability of the bearings to withstand localized stresses well above the code allowable. Detailed drawings of the test fixture and model have been completed and required material procured.

2. GCFR IRRADIATION EXPERIMENTS

In the joint ORNL-GA-ANL irradiation testing program for the evaluation of (U,Pu)O₂-fueled metal-clad fuel rods for the Gas-Cooled Fast Reactor (GCFR), emphasis continues on the testing of fuel rods of the vented-and-pressure-equalized concept, the reference GCFR fuel rod design. This vented design eliminates large pressure differentials across the cladding by venting the fuel rod interior to a pressure equalization system. The GCFR fuel-rod testing program benefits from information being developed in LMFBR programs and vice versa, since the fuel and cladding materials and many of the operating conditions are similar for the two reactors.

The current irradiation test, capsule GB-10, and the preceding one, capsule GB-9, were designed to permit an initial evaluation of the overall performance of the vented-and-pressure-equalized fuel rod. Many important aspects of the GCFR pressure equalization system are also being

studied in these tests. Test conditions are typical of those expected for a GCFR fuel rod except for the absence of significant fast-neutron exposure.

Postirradiation examination of the GB-9 fuel rod, which was irradiated at 48.6 kW/m (14.8 kW/ft) peak power level to a fuel burnup of approximately 54 MWd/kg heavy metal, was completed at ANL and GA during the current report period. The GB-9 experiment, being the first of a kind, yielded a large amount of information on the fission product release and fuel behavior to be expected with the GCFR vented fuel rod.

The GB-10 fuel rod, similar to GB-9 but designed with greater capability for measuring fission product release and transport, is being operated in three successive peak power steps: 39.4, 44.3, and 48.6 kW/m (12, 13.5, and 14.8 kW/ft). Sweep line connections to the GB-10 rod permit fission product release measurements under a number of different sweep flow modes, including passage of the sweep through the entire rod, simulating a leak in the cladding of a GCFR rod. The main objective of GB-10 is to obtain detailed information on the release, transport, and trapping of gaseous and volatile fission products. Another objective is to observe the general performance of the rod and to look for any behavior that could be a potential problem for the GCFR. Additional useful information is to be obtained later from postirradiation examination of the rod.

The GB-10 measurements and goals were expanded during the current report period. A large effort was devoted to the design and installation of three new systems to increase GB-10 capability for study of fission product release and transport. These were an on-line Ge(Li) gamma-ray detector system, which permits easier and more detailed fission gas release measurements, a system for taking large gas samples for the determination of stable noble gas release rates using mass spectrometry, and a system for measuring tritium release rates. Also, instrumentation is being evaluated and calibrated for future measurements of the H₂ and H₂O impurity levels in the sweep gas.

GB-10 operation continued satisfactorily at the intermediate fuel-rod power level of 44.3 kW/m (13.5 kW/ft) during the report period. Fuel burnup progressed from 35 to 70 MWd/kg. The burnup goal was revised from its original value of 75 to 100 MWd/kg.

Fuel-rod flow conductance measurements at 44.3 kW/m indicate an exponential decline in conductance with increasing burnup. Conductance improves significantly when the power is lowered.

Steady-state release rates for the gamma-emitting fission gases were measured at 44.3 kW/m under the planned sweep flow modes as a function of time, and as a function of operating pressure in a special test. Release data obtained at reduced pressures of 1.48 and 2.17 MPa (200 and 300 psig) when compared with the release data at the normal

operating pressure of 7.0 MPa (1000 psig) show the strong suppressing effect of high pressure on the steady-state venting of gases from the rod. There was an unexpected effect of helium pressure on release from the oxide fuel as well as the expected effect on gas-phase transport.

Stable noble gas release rates were measured at 44.3 kW/m; however, the data revealed a problem in the sampling or analysis techniques. The sampling system is currently being modified in an attempt to ensure representative samples.

The tritium monitoring system was operated with calibration gas in June, 1975. The system is apparently operating as designed, promising excellent sensitivity for measuring tritium in the capsule gas.

Plans for the remainder of the GB-10 irradiation include operation at the final power level of 48.6 kW/m (14.8 kW/ft), tritium monitoring experiments, and several additional special tests, including power cycling near the end of the irradiation.

3. GCFR CORE FLOW TEST LOOP

The test objectives and requirements for the GCFR Core Flow Test Loop were defined. A conceptual design and cost estimate were prepared, and additional cost estimates based on test bundle heater capacity, kind of funding, and construction starting dates were submitted to ERDA. Appropriate documentation such as the Conceptual Design Report, Construction Project Data Sheet, Project Management Plan, Quality Assurance Program Index (QAPI), Codes and Standards Proposal, and Sections 1 and 2 of the System Design Description was prepared.

Dynamic simulations made with a computer indicated that satisfactory loop performance could be achieved with a test loop configuration using two heat exchangers and three fast-acting valves to control coolant helium flow during transient operation.

Conceptual design layouts were made of the satisfactory loop configuration utilizing existing available space in Building 9201-3 in the Y-12 Plant. Preparation of preliminary specifications was initiated for heat exchangers, helium purification, and helium supply. A conceptual design was made of the test vessel that houses the test bundles, and a proposal for the codes and standards to be applied to the construction of the CFTL pressure boundaries was prepared. Pressure drops in the conceptual design loop were calculated, including the inlet plenum in the test vessel and the turning of the flow of coolant helium into the test bundle proper.

After preparation of a tentative specification for a helium circulator, a preliminary inquiry was made, by the UCND Purchasing Division, of the U.S. compressor industry to elicit interest in providing the CFTL circulator equipment. A specification for centrifugal circulators supported on gas-film-lubricated bearings was offered to the three U.S. manufacturers who indicated interest in the preliminary inquiry. After an evaluation team reviewed the bids and visited the manufacturers, a contract was awarded to Mechanical Technology Inc., Latham, New York, to produce the circulators.

A specification was prepared for an electric cartridge heater to simulate GCFR fuel pins in CFTL test bundles. A quality assurance plan for the heaters was prepared. Four vendors are making prototypes of the heater. The test section in the existing LMFBR Heater Test Facility was modified to accommodate a single CFTL test bundle heater. Low-power heaters were purchased for a seven-heater test to characterize electromagnetic interference noise on thermocouples installed inside test bundle heaters.

Plans were made to evaluate three candidate power supply and control schemes for the test bundle. A test with a bundle of seven low-power heaters was designed to obtain data on the effect of electromagnetic interference associated with each candidate scheme on the temperature signal from thermocouples installed inside heaters. The effects of operation of the heater at design power and temperature on electromagnetic interference noise will be evaluated in the modified LMFBR Heater Test Facility with heater prototypes.

Measurement and control flow diagrams were made for the conceptual loop design. Work on filters to eliminate or reduce satisfactorily the electromagnetic interference noise on heater thermocouples was initiated after preliminary work indicated that the existence of noise is probable. A switching system to add, subtract, or remove individual transformer windings in a power circuit was devised as one of the three candidate power and control schemes for the test bundle heaters. Work was initiated on the preliminary design of a controller to regulate test bundle power and coolant flow rate and cooling air flow rates to the two heat exchangers during fast transient operation. A preliminary specification for a gas chromatograph to analyze the CFTL helium was prepared. An ultrasonic device and a combination of venturi tubes and target flowmeters are being considered to measure the coolant helium flow rate to the test bundle. The insulation resistance of small-diameter sheathed thermocouples was measured under various operating temperature conditions, and the accuracy of the temperature signal from small-diameter sheathed thermocouples made of Chromel and Alumel was measured against Pt-Rh vs Pt thermocouples at temperatures up to 1000°C.

4. GCFR SHIELDING STUDIES

Analytical efforts to date include the identification of areas for concern in the GCFR Shielding model supplied by GAC and their incorporation into the GCFR shielding program plan, the development of cross-section sets, and their application in parametric studies to determine desirable computational models for GCFR calculations. These cross sections and modeling approaches were then applied in a cross-section sensitivity study for high-energy flux transmission to the PCRV liner, and to evaluate the effects of proposed revisions to the outer radial shield. The results indicate that the prospects for reduction of the outer radial shield, and therefore the reactor cavity radius, are reasonably favorable from the shielding point of view.

Experimental efforts were directed toward a basic rod streaming experiment. Experimental configurations were designed for single-cell and multiple-rod array cylindrical geometries with approximately 50% rod fraction by volume. Measurements for the single-cell pin streaming experiment were completed during June, with the full array experiment scheduled to be completed this summer. Pre-analysis of the single-cell experiment showed that significant high-energy neutron streaming should be observed with both UO_2 and iron rods. Two-dimensional discrete ordinates calculations were performed in the adjoint mode for a homogeneous full array configuration to provide bias factors for future Monte Carlo calculations of explicit multiple-pin array configurations.

5. DYNAMIC MODELING OF THE GCFR STEAM GENERATOR

The final refinements to a dynamic model of the GCFR steam generator were completed.

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1. GCFR PRESSURE VESSEL AND CLOSURE STUDIES

**J. P. Callahan, M. H. Raftenberg,
and G. C. Robinson**

1.1 INTRODUCTION

As an active participant in the Gas-Cooled Fast Reactor Development Program, the Oak Ridge National Laboratory has been assigned the task of investigating the performance of the closure plugs for both the central reactor cavity and the steam generator cavities as designed by General Atomic Company (GA).

Figure 1.1 shows a preliminary design of the Prestressed Concrete Reactor Vessel (PCRV) for the 300 MW(e) GCFR Demonstration Power Plant. A diametral cut has been made through the cylindrical PCRV, exposing the central reactor core cavity, one of the three steam generator cavities on the right, and one of three auxiliary circulator cavities on the left.

The reactor cavity closure plug can be seen directly above the core. At the upper right, directly above the main helium circulator, is the steam generator cavity closure plug.

Under normal operation of the plant, the helium coolant circulates downward through the core, upward around the bottom of the core thermal shielding, and out to the steam generator cavity. It then circulates downward through the steam generator and back up around the outside of the steam generator to the main helium circulators, where it is pumped to the top of the core cavity to complete the flow loop. The closures are designed to resist the mechanical loading of the pressurized coolant.

A preliminary cross section of the reactor cavity plug is shown in Fig. 1.2. It will have an array of axial penetrations for control rod insertion. The primary hold-down system at the top of the plug consists of a segmented wedge and toggle assembly that transmits the forces from the plug at one end to heavy forgings anchored by prestressing tendons at the other end. The secondary hold-down system consists of steel bayonets protruding from the outer liner of the plug that lock with matching bayonets on the closure wall of the vessel in the event of primary system failure.

Fig. 1.1 Demonstration 300-MW(e) GCFR Power Plant, Cross-Sectional View of Preliminary Design by General Atomic. To convert dimensions to meters, multiply numbers of feet by 0.305 and inches by 0.0254.

Fig. 1.2. Reactor Cavity Closure Plug for GCPR 300-MW(e) Demonstration Plant Cross-Sectional View of First Design Supplied by General Atomic. Penetration dimensions are 3.05 by 3.83 m.

The helium seal under normal operation is at the plug top forging near the primary hold-down system. The flow restrictor, similar to a piston ring, fits into an indentation in the plug and serves to restrict gas flow in the event of failure of the seal. The same type of hold-down system and seal are used for both closures.

Figure 1.3 shows the initial design of the steam generator cavity closure plug as supplied by GA. This plug has a central penetration through which two concentric steam pipes pass. The inner pipe transports steam into the vessel to drive the main helium circulator, and the outer pipe carries steam from the steam generator to the power turbines. The main helium circulator is attached to the bottom of the plug.

Fig. 1.3. Steam Generator Cavity Closure Plug for 300-MW(e) Demonstration Plant. Cross-sectional view of first design supplied by General Atomic. Dimensions shown are 3.43, 3.86, and 1.42 m.

The linkage of the piping to the top of the inner liner of the plug serves only for alignment and supports no appreciable load.

A recently prepared plan for conducting the closure model tests lists these objectives:

1. determine the structural response of the closure plugs under design operating conditions, with emphasis on identifying critical regions of stress concentration;
2. verify the desired gradual structure response to overpressure;
3. identify crack development and propagation in the concrete during overpressurization;
4. determine ultimate pressure capacity, mode of failure, and factor of safety of the closures; and
5. develop and verify theories of failure and analysis methods.

The steam generator cavity closure plug was selected for the initial study primarily because it is simpler than the reactor cavity plug. The steam generator cavity plug has received virtually full attention thus far, with the effort being divided between analytical and experimental work.

The plan for the experimental investigation of the steam generator cavity closure is to test a 1/15-scale model of the plug at room temperature under pressure loading and then a 1/4-scale model to allow more detailed instrumentation and modeling of the closure.

The progress of this past year is divided into three basic tasks: (1) elastic analyses of the steam generator closure, (2) development of smearing analytical techniques, and (3) planning of experimental model tests. These tasks are discussed individually in detail.

1.2 ELASTIC ANALYSES OF THE STEAM GENERATOR CAVITY CLOSURE

Elastic axisymmetric analyses of three designs for the closure plug have been performed by use of the two finite-element codes CREEP-PLAST¹ and STATIC SAP.² The purpose of these analyses initially is to aid in designing the closure. Once the geometry is selected, elastic calculations are used to aid in positioning instrumentation and designing the 1/15-scale model experiment. Efforts are currently under way to

incorporate a more sophisticated material characterization and perform inelastic calculations of the experimental models to assess present analytical capability.

CREEP-PLAST is a two-dimensional finite-element code capable of solving problems with creep and plasticity. Its element library is limited to constant-strain triangles. The code was originally developed by Y. R. Rashid and subsequently modified¹ at ORNL. It has been documented and used extensively at ORNL and is accompanied by an excellent graphics program for output display. Slight modifications were necessary to accommodate composite material requirements of the closure plugs.

STATIC-SAP, developed at the University of California at Berkeley,² is capable of handling only elastic material behavior. This code allows for orthotropy in elastic properties, which is a convenient feature for the closure analyses. Its extensive element library contains several elements for solving three-dimensional problems. The two-dimensional elements used are four-node linear isoparametric quadrilaterals and constant-strain triangles. To gain familiarity with the use of STATIC-SAP and confidence in its ability to solve composite materials problems, a concrete-steel composite hollow cylinder under internal pressure and plane strain was modeled as shown in Fig. 1.4. The calculated stresses,

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Fig. 1.4. Composite Cylinder Under Internal Pressure and Pie-Shaped Finite-Element Model. The forces applied normal to the two exposed radial-axial faces by the remainder of the cylinder are not shown.

strains, and displacements were in excellent agreement with an exact closed-form solution and with the results of an earlier axisymmetric analysis made with CREEP-PLAST.

The first design of the plug (Fig. 1.3) was analyzed with CREEP-PLAST for the 10.08 MPa (1462 psi) design pressure and the primary hold-down system in operation. Figure 1.5 shows the finite element mesh and loading employed for the analysis. The mesh has 523 constant-strain triangle elements and 321 node points. Material properties used for the concrete are Young's modulus, $E^C = 41.38 \text{ GPa}$ ($6.0 \times 10^6 \text{ psi}$), and Poisson's ratio, $\nu^C = 0.22$; and for the steel, $E^S = 200.0 \text{ GPa}$ ($29.0 \times 10^6 \text{ psi}$) and $\nu^S = 0.29$. The forces applied to the plug by the helium circulator are represented by the concentrated force P_C in the lower left corner of the grid. This force is the result of pressure acting upward on the circulator. Internal pressure is applied to the plug at the bottom and along the outer surface (shown on the right side of Fig. 1.5). The contact stress σ_1 and σ_2 shown in the upper right corner represent the reactions of the primary hold-down system. These stresses are assumed uniform, and shearing stresses are neglected. The bolt force P_B , which restrains the hold-down system wedges, is determined from force equilibrium of the toggle wedge assembly. Rigid body motion has been eliminated by tying down the axial motion of a node at the upper right corner of the section.

Figure 1.6 is a contour plot of the largest principal compressive stress in the cross-sectional plane of the concrete. The maximum calculated concrete stress of approximately 20.7 MPa (3000 psi) was located along the outer liner at the flow restrictor penetration. The maximum calculated steel stress of 124.1 MPa (18,000 psi) occurred in the forging directly below the wedge restraining bolt.

A noteworthy result is that the largest value principal stress exhibited slightly tensile values at the top near the inside wall. This tension was of small magnitude and not very significant. An alarming result, however, is the appearance of shear stress above 6.9 MPa (1000 psi) at the concrete-steel liner interface between the flow restrictor and top forging. The integrity of the bond is doubtful under such high stress.

Fig. 1.5. Mesh Layout and Applied Loadings for GCFR Steam Generator Closure Plug Finite Element Analysis.

Fig. 1.6. Contour Plot for Maximum Compressive Principal Stress in the Cross-Sectional Plane in the Concrete Produced by 10.08 MPa (1462 psi) Design Pressure. Contours are labeled with compressive stresses in psi; to convert to MPa multiply by 0.006895.

Also, a potential buckling tendency upon overpressure was noted for the inner liner near the helium circulator attachment at the bottom of the plug.

Figure 1.7 shows the modified closure design, which incorporates a "shear console" consisting of a conical steel plate section attached to the outer liner by a series of steel gusset plates and struts. The shear console system, which is embedded in the concrete, is designed to reduce shear stress at the liner-concrete interface.

From the standpoint of analysis, the shear console system introduces a significant deviation from axial symmetry. The most direct approach is to perform a three-dimensional analysis of the plug, while taking advantage of the large degree of axial symmetry of the other parts of the problem. Another option is to continue with an axisymmetric analysis employing a technique known as "smearing,"³ whereby in the region of the plug spanned by the gusset plate, material properties are averaged in the circumferential direction. A homogeneous elastic orthotropic material replaces the steel-concrete composite in this region.

The plan of attack for a three-dimensional analysis would be to model a pie-shaped segment of the plug bounded by an axial-radial plane through the center of a gusset plate and an axial-radial plane through the center of the region midway to the adjacent gusset plate. All nodes would lie on one of several radial-axial planes, including the two bounding the region. The basic scheme is illustrated in Fig. 1.8. Although this three-dimensional approach appears to be straightforward, restrictions on aspect ratio of elements require a very dense mesh and therefore an extremely expensive and time-consuming analysis.

For this first phase of the analysis in which modifications of the geometry were anticipated, the smearing approach with a two-dimensional axisymmetric analysis was chosen. Section 1.3 discusses two methods of smearing developed for this application. Both methods consist of replacing the steel-concrete composite regions of the gusset plates and 38.1-mm thick (1.5-in.) struts by a single homogeneous but nonisotropic elastic material.

Fig. 1.7. Revised GCFR Steam Generator Cavity Closure Plug. To convert dimensions to meters, multiply numbers of feet by 0.305 and inches by 0.0254.

Fig. 1.8. Mesh for a Three-Dimensional Analysis.

The code STATIC-SAP has been used for the analysis of the modified closure. The finite-element mesh used for the analysis (Fig. 1.9) contains 1181 constant-strain triangle elements and 665 nodes. The 10.08 MPa (1462 psi) pressure is applied as shown in Fig. 1.9 along with the corresponding reactions σ_1 , σ_2 , P_B , and P_C . The hold-down system is represented by normal contact stresses σ_1 and σ_2 , and the axial bolt force P_B , is distributed evenly between four nodes. The force exerted by the circulator, P_C , is distributed between two nodes.

Four materials are present in the analytical model; steel [elastic constants $E^s = 200.0$ GPa (29.0×10^6 psi), $\nu^s = 0.29$], concrete [$E^c = 41.38$ GPa (6.0×10^6 psi), $\nu^c = 0.22$], a smeared material in the region spanned by the gusset plates and a smeared material in the region spanned by the struts. The first smearing method (see Method 1 of Sect. 1.3) was used to calculate the material constants. The results of

**Fig. 1.9. Mesh Layout and
Applied Loadings for GCFR Steam
Generator Closure Plug Finite-
Element Analysis.**

the analysis of the modified closure indicated that the shear console reduced the shear stress at the outer liner interface to below 3.45 MPa (500 psi) and strengthened the liner near the circulator attachment.

A final closure design (Fig. 1.10) was provided by CA, primarily to achieve greater simplicity for the initial experimental models. The modifications are as follows:

1. The thick-shell portion of the liner near the circulator attachment has been reduced.
2. The secondary hold-down system (bayonet lock) has been removed, and the plug geometry simplified.
3. The shear console struts have been eliminated, and the shear console is relocated at a lower level in the plug.
4. The flow restrictor indentation has been eliminated. The flow restrictor removal is significant since the indentation introduced a region of stress concentration under the action of the primary hold-down system.
5. An additional shear anchor (protrusion of the liner into the concrete) has been added to the inner liner.

Smeared axisymmetric elastic analyses have been performed for this design, both with and without the flow restrictor indentation. The finite element meshes are shown in Figs. 1.11 and 1.12 for the model with and without the flow restrictor indentation, respectively. The elements are mostly 4-node linear isoparametric quadrilaterals, with several constant-strain triangles included as needed for transition. A pressure loading of 10.08 MPa (1462 psi) (design pressure) has been applied to the bottom and outer surfaces of the plug, and a concentrated force representing the action of the helium circulator at this pressure has been applied to two nodes at the bottom of the inner liner, exactly as before. However, axisymmetric elements have been added to simulate the primary hold-down system shown in the upper right-hand corner of the two figures. Rigid body displacement is eliminated by tying down three nodes at the upper right end of the toggle.

Material constants for the smeared region spanned by the gusset plates are determined for the plug model geometry using the second and

Fig. 1.10. CCFR Steam Generator Cavity Closure Plug, Third Design, also the Model Test Design. To convert dimensions to meters, multiply numbers of feet by 0.305 and inches by 0.0254.

**Fig. 1.11. Finite-Element
Mesh of GCFR Steam Generator Cavity
Closure Plug (Third Design) with
Flow Restrictor Indentation.**

**Fig. 1.12. Finite-Element Mesh
of the GCFR Steam Generator Cavity
Closure Plug (Third Design) Without
Flow Restrictor.**

improved smearing method. (See Method 2 of Sect. 1.3.) Gravity has been included as a loading. Values for density used for steel and concrete, respectively, are 7850 kg/m^3 (0.2836 lb/in.^3) and 2323 kg/m^3 (0.08391 lb/in.^3). The density of the smeared region is 3012 kg/m^3 (0.1088 lb/in.^3). The results of this analysis are being used in designing the 1/15-scale model test of the steam generator cavity closure.

1.3 DEVELOPMENT OF SHEARING TECHNIQUES

In finite-element analyses of composite material structure, one frequently encounters structures whose outlines have an axis of symmetry, but with internal deviations in geometry from axial symmetry. In the case of the GCFR steam generator cavity closure plug, the shear console system represents a region with such internal deviation. In such cases, it is often worthwhile to extract information from an axisymmetric approach before proceeding with the more expensive three-dimensional analysis. "Smearing" refers to the process of devising an approximate axisymmetric model to a structure for this purpose.

Two methods of smearing suitable to the closure plug analysis are presented. Method 1 was developed first and was employed in the analysis of the second design of the steam generator cavity closure plug (Fig. 1.7). Method 2 is similar but significantly refined and was applied to the third design (Fig. 1.10).

1.3.1 Method 1

In this scheme transversely isotropic elements possessing five independent material constants are used to model the portion of the plug spanned by steel plate. The stress-strain relations take the form:

$$\epsilon_{\theta} = \frac{1}{E_1} \sigma_{\theta} - \frac{\nu_1}{E_1} \sigma_z - \frac{\nu_1}{E_1} \sigma_r, \quad (1a)$$

$$\epsilon_z = -\frac{\nu_1}{E_1} \sigma_{\theta} + \frac{1}{E_2} \sigma_z - \frac{\nu_2}{E_2} \sigma_r, \quad (1b)$$

$$\epsilon_r = -\frac{\nu_1}{E_1} \sigma_\theta - \frac{\nu_2}{E_2} \sigma_z + \frac{1}{E_2} \sigma_r, \quad (1c)$$

$$\gamma_{rz} = \frac{1}{G_2} \tau_{rz}. \quad (1d)$$

In these equations, ϵ_θ , ϵ_z , ϵ_r , and γ_{rz} represent hoop, axial, radial, and shear strains, respectively. Likewise, σ_θ , σ_z , σ_r , and τ_{rz} represent hoop, axial, radial, and shear stresses, respectively.

The material constants E_1 , ν_1 , E_2 , ν_2 , and G_2 characterize the transversely isotropic material. Young's modulus E_2 , Poisson's ratio ν_2 , and shear modulus G_2 govern behavior in the axial-radial plane. Behavior out of the plane is governed by Young's modulus E_1 and Poisson's ratio ν_1 . Values for the material constants are selected to ensure that these elements exhibit the same gross deformation characteristics under the most important loading conditions as the steel-concrete composite they represent.

Since the largest absolute value stress component in this problem is the hoop stress σ_θ , the condition of uniform hoop stress is considered initially. A composite steel (s) concrete (c) parallelepiped is shown loaded under uniform σ_θ , in Fig. 1.13(a). Note the lengths l , l^s , l^c , d , and h defined in this figure. The material constants in the steel region are Young's modulus E^s , Poisson's ratio ν^s , and shear modulus G^s . In the concrete region, the constants are Young's modulus E^c , Poisson's ratio ν^c , and shear modulus G^c . The resulting hoop strains in the steel and concrete, respectively, are

$$\epsilon_\theta^s = \sigma_\theta / E^s, \quad (2a)$$

$$\epsilon_\theta^c = \sigma_\theta / E^c. \quad (2b)$$

The corresponding elongations in steel and concrete, respectively, are

$$\delta_\theta^s = l^s \epsilon_\theta^s = \frac{l^s}{E^s} \sigma_\theta. \quad (3a)$$

Fig. 1.13. Parallelepipeds with Uniform σ_0 . (a) Composite. (b) Transversely isotropic.

$$\delta_{\theta}^c = l^c \epsilon_{\theta}^c = \frac{l^c}{E^c} \sigma_{\theta} . \quad (3b)$$

The gross elongation of the parallelepiped in the hoop direction δ_{θ}^T is then

$$\delta_{\theta}^T = \delta_{\theta}^s + \delta_{\theta}^c = \left(\frac{l^s}{E^s} + \frac{l^c}{E^c} \right) \sigma_{\theta} . \quad (4)$$

Define the effective hoop strain by

$$\epsilon_{\theta}^* = \delta_{\theta}^T / l . \quad (5)$$

Substituting Eq. (4) into (5),

$$\epsilon_{\theta}^* = \left(\frac{l^s}{E^s} + \frac{l^c}{E^c} \right) \sigma_{\theta} = \left(\frac{E^c l^s + E^s l^c}{E^s E^c} \right) \sigma_{\theta} , \quad (6)$$

where f^s and f^c are defined by

$$f^s = l^s/l, \quad (7a)$$

$$f^c = l^c/l. \quad (7b)$$

Next consider the parallelepiped of identical dimensions and composed of transversely isotropic material (TIM), described by Eqs. (1). This parallelepiped is also loaded under uniform σ_θ , as shown in Fig. 1.13(b).

From Eq. (1a) its resulting hoop strain is

$$\epsilon_\theta = \frac{1}{E_1} \sigma_\theta. \quad (8)$$

Comparing Eqs. (6) and (8), for

$$E_1 = \frac{E^s E^c}{f^s E^c + f^c E^s} \quad (9)$$

the two parallelepipeds will exhibit the same gross deformation in the hoop direction under the action of uniform hoop stress.

The second "loading" condition considered is that of uniform axial elongation δ_z with no deformation allowed in the hoop or radial directions. As shown in Fig. 1.14(a), the axial strain in both the steel and concrete portions of the parallelepiped, ϵ_z^s and ϵ_z^c , respectively, are given by

$$\epsilon_z^s = \epsilon_z^c = \delta_z/d \equiv \epsilon_z. \quad (10)$$

The resulting axial stresses in these steel and concrete regions are

$$\sigma_z^s = E^s \epsilon_z, \quad (11a)$$

$$\sigma_z^c = E^c \epsilon_z. \quad (11b)$$

The total axial reaction force (F_z) generated is then

$$F_z = \sigma_z^s l^s h + \sigma_z^c l^c h. \quad (12)$$

Fig. 1.14. Parallelepipeds with Uniform Axial Elongations, δ_z .
 (a) Composite. (b) Transversely isotropic.

Using Eqs. (7a), (7b), (11a), and (11b) in Eq. (12) gives

$$F_z = (f^S E^S + f^C E^C) l h \epsilon_z. \quad (13)$$

Define the effective axial stress by

$$\sigma_z^* = F_z / l h = (f^S E^S + f^C E^C) \epsilon_z. \quad (14)$$

For the transversely isotropic parallelepiped under the same "loading," as shown in Fig. 1.14(b), Eqs. (1) show the axial stress to be

$$\sigma_z = E_2 \epsilon_z. \quad (15)$$

Comparing Eqs. (14) and (15), for

$$E_2 = f^S E^S + f^C E^C \quad (16)$$

the two parallelepipeds will generate equivalent axial stresses in response to uniform axial strains. Note that if uniform radial strain were imposed, the same expression for E_2 [Eq. (16)] would result.

The third condition imposed is uniform shear strain γ_{rz} . The composite parallelepiped "loaded" under γ_{rz} is shown in Fig. 1.15(a). The shear strain in both the steel and concrete sections is

$$\gamma_{rz}^s = \gamma_{rz}^c = \gamma_{rz} , \quad (17)$$

where γ_{rz}^s and γ_{rz}^c are shear strains in the steel and concrete, respectively. The resulting shear stresses are

$$\tau_{rz}^s = G^s \gamma_{rz}^s = G^s \gamma_{rz} , \quad (18a)$$

$$\tau_{rz}^c = G^c \gamma_{rz}^c = G^c \gamma_{rz} . \quad (18b)$$

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Fig. 1.15. Parallelepipeds with Uniform Shear Strain γ_{rz} .
(a) Composite. (b) Transversely isotropic.

The total shear force F_{rz} generated by the parallelepiped as a reaction is

$$F_{rz} = \tau_{rz}^s l^s d + \tau_{rz}^c l^c d . \quad (19)$$

Substituting Eqs. (7a), (7b), (18a), and (18b) into Eq. (19) gives

$$F_{rz} = (f^s G^s + f^c G^c) l d \gamma_{rz} . \quad (20)$$

The effective shear stress τ_{rz}^* is defined by

$$\tau_{rz}^* = \frac{F_{rz}}{l d} = (f^s G^s + f^c G^c) \gamma_{rz} = \left[\frac{f^s E^s}{2(1 + \nu^s)} + \frac{f^c E^c}{2(1 + \nu^c)} \right] \gamma_{rz} . \quad (21)$$

For the transversely isotropic parallelepiped shown in Fig. 1.15(b) the shear stress in response to the uniform γ_{rz} imposed is, by Eq. (1d),

$$\tau_{rz} = G_2 \gamma_{rz} . \quad (22)$$

Comparing Eqs. (21) and (22),

$$G_2 = \frac{f^s E^s}{2(1 + \nu^s)} + \frac{f^c E^c}{2(1 + \nu^c)} \quad (23)$$

guarantees the proper response.

To complete the scheme, simply use the Poisson's ratio of concrete; namely,

$$\nu_1 = \nu_2 = \nu^c . \quad (24)$$

Equations (9), (16), (23), and (24) constitute the smearing scheme that was used in analyzing the second geometry of the plug. The equations were applied to both the 32-mm-thick (1 1/4-in.) gusset plate region and the 38-mm-thick (1 1/2-in.) strut region. The values used for the Poisson's ratios are $\nu_1 = \nu_2 = 0.22$. The elastic moduli obtained are in Table 1.1.

Table 1.1. Elastic Moduli Obtained by Method 1 Smearing

Modulus	Value for Gusset Plate Region		Value for Strut Region	
	(GPa)	(psi)	(GPa)	(psi)
E_1	61.175	8.8704×10^6	62.058	8.9984×10^6
E_2	45.925	6.6591	46.151	6.6919
G_2	24.517	3.5549	24.854	3.6038

1.3.2 Method 2 - General Principles

This second method represents a refinement to the first. A more methodical method for applying load cases to the finite element model has been incorporated. The smeared region is represented as an orthotropic elastic material described by seven independent material constants, as opposed to the simpler transversely isotropic representation of Method 1 having only five independent constants. The method will be described first in generality, and later applied to the closure plug study.

A general procedure for smearing in finite element structural analysis will be presented here. It is valid for structures with two necessary geometric properties; (1) the outline of the structure must be axisymmetric, and (2) in the region or regions of the structure in which deviations from axial symmetry exist, the geometry must vary circumferentially about the axis of symmetry of the outline with a rapid periodicity.

Figure 1.16 shows a simple structure that satisfies these requirements. A cylindrical r - θ - z coordinate system is implied in the figure, with the z axis being the axis of symmetry of the structure's outline. Area S is the projection of the region in which geometry varies circumferentially. The condition of rapid periodicity requires that a large number of planes of symmetry exists. These planes of symmetry bound pie-shaped substructures, with the entire structure being a repetition of the geometry of these substructures.

Fig. 1.16. A Structure with a Symmetric Outline. (a) Cross section. (b) View from above.

In performing an axisymmetric finite-element analysis of the structure in Fig. 1.16, an element mesh is laid out on the plane projection to cover the area to the right of the axis of symmetry (Fig. 1.17). A group of these elements covers region S , in which shearing must be performed. For each element, the stress-strain relationship is described in terms of a material matrix $[D]$ defined by

$$\{\sigma\} = [D]\{\epsilon\} \quad (25a)$$

where $\{\sigma\}$ and $\{\epsilon\}$ are column vectors of stresses and strains, respectively, for the element.⁴ In the axisymmetric problem, there are the three normal stresses — radial σ_r , axial σ_z , and hoop σ_θ — and one shear component τ_{rz} . The two shear components $\tau_{r\theta}$ and $\tau_{\theta z}$ are identically zero.

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Fig. 1.17. Finite-Element Discretization of the Cross Section.

Similarly, there are four components of strain ϵ_r , ϵ_z , ϵ_θ , and γ_{rz} . Then $[D]$ is a 4×4 matrix. Using an indexial notation for stress and strain components in which 1, 2, and 3 are associated with r , z , and θ , respectively, Eq. (25a) can be written more explicitly as

$$\begin{Bmatrix} \sigma_1 \\ \sigma_2 \\ \sigma_3 \\ \tau_{12} \end{Bmatrix} = \begin{bmatrix} D_{11} & D_{12} & D_{13} & D_{14} \\ D_{21} & D_{22} & D_{23} & D_{24} \\ D_{31} & D_{32} & D_{33} & D_{34} \\ D_{41} & D_{42} & D_{43} & D_{44} \end{bmatrix} \begin{Bmatrix} \epsilon_1 \\ \epsilon_2 \\ \epsilon_3 \\ \gamma_{12} \end{Bmatrix} \quad (25b)$$

Each column of $[D]$ has a simple physical significance. The first column $[D_{11} \ D_{21} \ D_{31} \ D_{41}]^T$ is the stress response to the "unit strain loading" $\epsilon_1 = 1$, $\epsilon_2 = \epsilon_3 = \gamma_{12} = 0$, or $\{\epsilon\} = [1 \ 0 \ 0 \ 0]^T$. In the same manner, each of the remaining three columns can be viewed as the stress response associated with a unit strain loading.

This concept will be used in smearing to produce a $[D]$ matrix for the elements in S . A homogeneous orthotropic material will be introduced to approximately represent this region in which material properties actually vary circumferentially.

The matrix $[S]$, defined as the inverse of $[D]$, also relates stress and strain for an element by

$$\begin{Bmatrix} \epsilon_1 \\ \epsilon_2 \\ \epsilon_3 \\ \gamma_{12} \end{Bmatrix} = \begin{bmatrix} S_{11} & S_{12} & S_{13} & S_{14} \\ S_{21} & S_{22} & S_{23} & S_{24} \\ S_{31} & S_{32} & S_{33} & S_{34} \\ S_{41} & S_{42} & S_{43} & S_{44} \end{bmatrix} \begin{Bmatrix} \sigma_1 \\ \sigma_2 \\ \sigma_3 \\ \tau_{12} \end{Bmatrix} \quad (26)$$

An orthotropic elastic material is described by the independent material constants E_{11} , E_{22} , E_{33} , ν_{12} , ν_{13} , ν_{23} , G_{12} that appear in the stress-strain relationship⁵

$$\begin{Bmatrix} \epsilon_1 \\ \epsilon_2 \\ \epsilon_3 \\ \gamma_{12} \end{Bmatrix} = \begin{bmatrix} 1/E_{11} & -\nu_{12}/E_{22} & -\nu_{13}/E_{33} & 0 \\ & 1/E_{22} & -\nu_{23}/E_{33} & 0 \\ & & 1/E_{33} & 0 \\ & \text{symmetric} & & 1/G_{12} \end{bmatrix} \begin{Bmatrix} \sigma_1 \\ \sigma_2 \\ \sigma_3 \\ \tau_{12} \end{Bmatrix} \quad (27)$$

The axes of material orthotropy are x_1 , x_2 , and x_3 .

Comparing Eqs. (26) and (27), these constants are related to the members of $[S]$ by

$$\begin{aligned} E_{11} &= 1/S_{11} \\ E_{22} &= 1/S_{22} \\ E_{33} &= 1/S_{33} \\ \nu_{12} &= -E_{22}S_{12} = -S_{12}/S_{22} \\ \nu_{13} &= -E_{33}S_{13} = -S_{13}/S_{33} \\ \nu_{23} &= -E_{33}S_{23} = -S_{23}/S_{33} \\ G_{12} &= 1/S_{44} \end{aligned} \quad (28)$$

1.3.3 Method 2 - Procedure

The conceptual stages in the smearing process will now be described. Figure 1.18(a) shows a pie-shaped substructure bounded by two adjacent planes of symmetry intersecting at a small angle α . In Fig. 1.18(b), a small body with centroid at (r, z) and bounded by the planes of symmetry

Fig. 1.18. Conceptual Stages in Deriving a Unit Cube.

is extracted. Two faces are squares with sides of length ra . Next the body is scaled up to unit dimensions [Fig. 1.18(c)]. Since a is small, this body can be closely approximated by the cube of unit dimensions shown in Fig. 1.18(d).

This unit cube is now constructed with parallelepiped finite elements (Fig. 1.19). The internal element structure of the cube is as detailed as is required to adequately model the geometry of all or some of the region spanned by S . The six faces of the unit cube are labeled as follows: $F1$ ($x = 1$), $F2$ ($y = 1$), $F3$ ($z = 1$), $F4$ ($x = 0$), $F5$ ($x = 0$), $F6$ ($z = 0$). Each solid element composing the unit cube is a six-sided parallelepiped with faces f_1 , f_2 , f_3 , f_4 , f_5 , and f_6 .

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Fig. 1.19. Finite Element Subdivision of the Unit Cube.

The unit cube is considered to be described by a $[D]$ matrix through

$$\begin{Bmatrix} \sigma_y \\ \sigma_z \\ \sigma_x \\ \tau_{yz} \end{Bmatrix} = \begin{bmatrix} D_{11} & D_{12} & D_{13} & D_{14} \\ D_{21} & D_{22} & D_{23} & D_{24} \\ D_{31} & D_{32} & D_{33} & D_{34} \\ D_{41} & D_{42} & D_{43} & D_{44} \end{bmatrix} \begin{Bmatrix} \epsilon_y \\ \epsilon_z \\ \epsilon_x \\ \gamma_{yz} \end{Bmatrix}. \quad (29)$$

Comparing Eqs. (29) and (25b), σ_y , σ_z , σ_x , and τ_{yz} are playing the roles of σ_1 , σ_2 , σ_3 , and τ_{12} , respectively. Similarly, ϵ_y , ϵ_z , ϵ_x , and γ_{yz} play the roles of ϵ_1 , ϵ_2 , ϵ_3 , and γ_{12} , respectively.

Now by using a finite-element code to successively apply the four unit strain loadings to the unit cube and by examining some suitable definition of the stress vector $[\sigma_y \ \sigma_z \ \sigma_x \ \tau_{yz}]^T$ for the cube, values can be assigned to $[D]$ column by column. The technique has been previously described and implemented by Ross.⁶ In this earlier work, the technique was used to derive macroscopic orthotropic material properties for use in the three-dimensional analysis of composites.

The remainder of the procedure is divided into three stages:

(1) application of the unit strain loadings to the finite element model, (2) calculation of the stress components on the cube in response to each unit strain loading, and (3) calculation of material constants needed for description of the smeared orthotropic material for a subsequent axisymmetric analysis. The steps in this procedure have been previously described explicitly⁶ and are repeated here in light of this new application. In a later section, the procedure will be illustrated with a particular example.

1.3.3.1 Application of the Unit Strain Loadings to the Finite-Element Model

A finite element code is used to apply the four unit strain loading cases to the cube and investigate the stress response. The procedure is now explained.

Normal strains. The three normal strain loads — ϵ_x , ϵ_y , and ϵ_z — are applied in an identical fashion. The procedure will be described for the case of ϵ_x and can be extended to the other cases by analogy.

Referring to Fig. 1.19, displace all nodes lying on F1 one unit in the positive x direction. Simultaneously, restrain all nodes lying on F2, F3, F4, F5, and F6 from displacement normal to their respective faces. No other restraints are placed on any nodes. The resulting configuration, shown in Fig. 1.20, is described by the strain state $\epsilon_y = 0$, $\epsilon_z = 0$, $\epsilon_x = 1$, $\gamma_{yz} = 0$ as is desired.

Fig. 1.20. Application of the Unit Strain $\epsilon_x = 1$, $\epsilon_y = 0$, $\epsilon_z = 0$, $\gamma_{yz} = 0$.

Shear strain. The procedure for applying a unit shear γ_{yz} is now described. Referring again to Fig. 1.19, displace all nodes on F2 a distance 0.5 in the positive z direction, and displace all nodes on F3 a distance 0.5 in the positive y direction. Restrain all nodes on F1 and F4 from displacement normal to their faces (x direction). Also, restrain all nodes on F6 from displacement in the y direction and all nodes on F5 from displacement in the z direction.

The configuration of the cube as a result of this process is shown in Fig. 1.21. The strain state is $\epsilon_y = 0$, $\epsilon_z = 0$, $\epsilon_x = 0$, and $\gamma_{yz} = 1$.

For the next stage in the process, it is convenient to have the finite-element code output stresses at the centroid of each of the six faces of each element. The code STATIC SAP by Wilson² has this capability and has been used. The code was modified slightly to have the output punched on IBM cards.

Fig. 1.21. Application of the Unit Strain $\epsilon_x = 0$, $\epsilon_y = 0$, $\epsilon_z = 0$, $\gamma_{yz} = 1$.

1.3.3.2 Calculation of the Stress Components on the Cube in Response to Each Unit Strain Loading

In this second stage average stresses on the faces of the cube are computed from the output from stage 1 for each of the four loadings. Then for each loading, a column of the $[D]$ matrix is determined. Some notation is now introduced.

Let $a_{i,j}$ be the surface area of element i contributing to the face F_j of the unit cube. The index i ranges from 1 to M , where M is the total number of elements in the cube. Also, j ranges from 1 to 6, corresponding to the six faces of the cube. Thus $a_{i,j}$ equals the area of f_j of the element for those elements that lie on F_j of the cube. For elements not lying on F_j , $a_{i,j} = 0$. For example, referring to Fig. 1.22, $a_{i,1}$ has a value equal to the surface area of f_1 of element i , whereas $a_{i+1,1} = 0$.

Fig. 1.22. $a_{i,1}$ is Shown for the Unit Cube.

The quantity A_j is the net surface area of F_j of the unit cube. It is the sum of the contribution of the M elements:

$$A_j = \sum_{i=1}^M a_{i,j}, \quad j = 1, 2, \dots, 6 \quad (30)$$

We let $\sigma_{i,j,k,l}$ represent a component of stress at the centroid of f_j of element i in response to unit strain load case l , which ranges from 1 to 4. As before, i ranges from 1 to M and j from 1 to 6. The subscript k specifies the component of the stress tensor at that point, expressed in terms of the x,y,z coordinate system of Fig. 1.19. The six components are $\sigma_y, \sigma_z, \sigma_x, \tau_{yz}, \tau_{xy}$, and τ_{xz} for which k has values 1, 2, 3, 4, 5, 6, respectively. Thus $\sigma_{2,3,3,2}$ represents the value of σ_x at the centroid of face 3 of element 2 in response to unit strain loading 2, which is $\epsilon_y = 0, \epsilon_z = 1, \epsilon_x = 0, \gamma_{yz} = 0$.

Unit normal strain loadings. For each of the three normal unit strain loadings ($l = 1, 2, 3$), a column of 3 elements of $[D]$, $[D_{1l} \ D_{2l} \ D_{3l}]^T$, gets determined by computing the average normal stresses on the faces of the unit cube.

Let $\bar{\tau}_{j,l}$ be the net normal force acting on F_j of the unit cube in response to load l . It is computed by summing the normal forces on F_j of all elements lying on F_j of the cube.

$$\bar{\tau}_{1,l} = \sum_{i=1}^M a_{i,1} \sigma_{i,1,3,l}, \quad l = 1, 2, 3; \quad (31a)$$

$$\bar{\tau}_{j,l} = \sum_{i=1}^M a_{i,j} \sigma_{i,j,j-1,l}, \quad j = 2, 3, 4; \quad l = 1, 2, 3. \quad (31b)$$

$$\bar{\tau}_{j,l} = \sum_{i=1}^M a_{i,j} \sigma_{i,j,j-4,l}, \quad j = 5, 6, \quad l = 1, 2, 3. \quad (31c)$$

Let $\Sigma_{j,l}$ be the net normal stress on F_j of the unit cube for load case l , or

$$\Sigma_{j,l} = \bar{\tau}_{j,l} / A_j, \quad j = 1, 2, \dots, 6, \quad l = 1, 2, 3. \quad (32a)$$

Stresses on opposite sides are averaged to get D_{jl} . The nine members of $[D]$ determined from the three normal unit strains are then

$$D_{jl} = (\Sigma_{j,l} + \Sigma_{j+3,l}) / 2, \quad j = 1, 2, 3, \quad l = 1, 2, 3. \quad (33a)$$

Unit shear strain loading. The fourth load case ($l = 4$) is used to determine D_{44} , relating τ_{12} and γ_{12} . The net shearing force on the cube involving τ_{12} is

$$S_j = \sum_{i=1}^M a_{i,j} \sigma_{i,j,4,4}, \quad j = 2, 3, 5, 6. \quad (31e)$$

The average shearing stress on these faces is

$$\Sigma_{j,4} = S_j/A_j, \quad j = 2,3,5,6. \quad (32b)$$

Then D_{44} is the average of the shearing stresses on these four faces.

$$D_{44} = (\Sigma_{2,4} + \Sigma_{3,4} + \Sigma_{5,4} + \Sigma_{6,4})/4. \quad (33b)$$

The remaining six members of $[D]$ are assigned the value zero, consistent with the representation as an orthotropic elastic material with axes of material orthotropy coincident with the $r-\theta-z$ axes of the structure. So

$$D_{j4} = 0, \quad j = 1,2,3, \quad (33c)$$

$$D_{4l} = 0, \quad l = 1,2,3. \quad (34d)$$

The resulting $[D]$ matrix will be found to be symmetric, with seven independent members reflecting the seven independent elastic constants needed to characterize a homogeneous orthotropic elastic material:

$$[D] = \begin{bmatrix} D_{11} & D_{12} & D_{13} & 0 \\ & D_{22} & D_{23} & 0 \\ & & D_{33} & 0 \\ \text{symmetric} & & & D_{44} \end{bmatrix}, \quad (34)$$

1.3.3.3 Calculation of Material Constants Needed for Description of the Smeared Orthotropic Material for Subsequent Axisymmetric Analysis

First $[S]$ is computed as the inverse of $[D]$ just obtained:

$$[S] = [D]^{-1}. \quad (35)$$

The seven familiar elastic constants are computed from $[S]$ using Eqs. (28). These are used for material description of the elements in $[S]$ of the structure to be analyzed axisymmetrically.

1.3.4 Application of Method 2 to the Steam Generator Cavity Closure Plug Analysis

The third design of the closure plug has been described in Sect. 1.2. The design is shown in Fig. 1.10 and the meshes used for the axisymmetric solutions are in Figs. 1.11 and 1.12.

In the central portion of the plug, an axisymmetric steel conical section is attached to the liner at the outside wall by forty 32-in.-thick (1 1/4-in.) gusset plates. These plates introduce a significant deviation from axial symmetry into the problem and necessitate smearing. The r - z planes that pass through the center of these plates are planes of symmetry, as are the r - z planes that are equidistant from two adjacent plates. In fact, the plug can be considered as a periodic replication of pairs of mirror-image pie-shaped segments, each bounded by two adjacent planes of symmetry, intersecting at an angle of 4.5° . This illustrates the type of structure for which the method has been devised.

The code used for both the smearing analysis of the unit cube and the subsequent axisymmetric analysis of the plug was STATIC SAP.² It is capable of handling general orthotropic elastic materials, and is suitable for the analysis of the smeared structure.

A unit cube was constructed with 12 eight-node brick linear isoparametric elements (Fig. 1.23). The four steel elements representing the

Fig. 1.23. The Unit Cube Used for Smearing in the Gusset Plate Region.

gusset plate were assigned material properties $E^S = 200.0$ GPa (29.0×10^6 psi), $\nu^S = 0.29$. Of the x (circumferential) dimension 12.48% traverses steel. This is a reasonably accurate estimate for the entire region S , so the $[D]$ matrix obtained from this cube was used for all the elements. For the remaining eight elements representing the concrete, the material constants used were $E^C = 41.4$ GPa (6.0×10^6 psi), $\nu^C = 0.22$.

After the four unit strain loadings were applied, $[D]$ was obtained as

$$[D] = \begin{bmatrix} 69.70 & 20.94 & 15.67 & 0 \\ 20.94 & 69.70 & 15.67 & 0 \\ 15.67 & 15.67 & 52.62 & 0 \\ 0 & 0 & 0 & 24.52 \end{bmatrix} \text{ GPa} \quad (36)$$

$$= \begin{bmatrix} 10.1456 & 3.0365 & 2.2724 & 0 \\ 3.0365 & 10.1456 & 2.2724 & 0 \\ 2.2724 & 2.2724 & 7.6300 & 0 \\ 0 & 0 & 0 & 3.5547 \end{bmatrix} \times 10^6 \text{ psi} .$$

Then $[D]$ is inverted to get $[S]$, from which the seven material constants are found to be

$$\begin{aligned} E_{11} &= E_{22} = 61.25 \text{ GPa } (8.8807 \times 10^6 \text{ psi}) , \\ E_{33} &= 47.22 \text{ GPa } (6.8465 \times 10^6 \text{ psi}) , \\ \nu_{12} &= 0.24921 , \\ \nu_{13} &= \nu_{23} = 0.17239 , \\ G_{12} &= 24.52 \text{ GPa } (3.5547 \times 10^6 \text{ psi}) . \end{aligned} \quad (37)$$

Note that modulus E_{33} relating hoop stress to hoop strain, components normal to the gusset plate, has a value only slightly higher than that used for the concrete. Moduli E_{11} and E_{22} , involving normal stresses and strains in the plane of the plate, have higher values. Thus the presence of steel is felt more in this plane.

1.3.5 Smearing: Conclusions and Assessment

An approximate axisymmetric solution to a complex structural problem was obtained at a nominal cost by use of smearing. Assuming the structure satisfactorily meets the two stipulated requirements, the smearing technique labeled Method 2 ensures that at any point (r, z) in the cross section, the solution obtained gives the correct stresses, strains, and displacements averaged over the circumference.

The question that then arises is to what extent the stresses, strains and displacements at some particular circumferential location θ might deviate from the computed averaged values. This question is difficult to answer with generality. For a given problem, however, much light can be shed on this matter by performing a two-dimensional plane stress or plane strain analysis (whichever more closely approximates the problem) of a cross section described by $z = \text{constant}$ and passing through the region S in which smearing was performed. The procedure conceptually (Fig. 1.24) is to expose a circular cross section of interest, $z = \text{constant}$, and lay out a two-dimensional mesh on the cross-sectional area bounded by two adjacent planes (lines) of symmetry.

The result will give a good indication of the expected deviation at different values of θ from the axisymmetric solution. The deviation will be most significant at radial positions r that lie in the smeared composite region where finite jumps occur in certain stress and strain quantities at material interfaces.

Stress and strains in the individual material regions that compose the composite region replaced by a homogeneous smeared material in the analysis are estimated numerically by treating quantities that are continuous in this region as being constants (independent of θ). Those quantities that experience a discontinuity at a material interface are treated as constants within a given material, but the jump in value at the interface is taken into account.

This has also been applied to the plug structure of Fig. 1.10. Since the normal to the gusset plates in this problem is approximately in the circumferential direction, hoop stress σ_θ is continuous across the steel-concrete interface. Strains in the plane of this interface — ϵ_r , ϵ_z , and γ_{rz} — are also continuous. Assuming these four quantities

Fig. 1.24. A Plane Stress or Plane Strain Analysis to Estimate Circumferential Variation in Behavior. (a) Cross section imposed with $z = \text{constant}$. (b) Mesh laid out for a two-dimensional analysis.

to be constant, they are output directly from the axisymmetric analysis. The discontinuous quantities are stresses σ_r , σ_z , and τ_{rz} and hoop strain ϵ_θ . Designate these as σ_r^c , σ_z^c , τ_{rz}^c and ϵ_θ^c in the concrete and σ_r^s , σ_z^s , τ_{rz}^s and ϵ_θ^s in the steel, all constants within their respective materials.

Hooke's law in the concrete gives

$$\epsilon_r = \frac{1}{E^c} \sigma_r^c - \frac{\nu^c}{E^c} \sigma_z^c - \frac{\nu^c}{E^c} \sigma_\theta^c, \quad (38a)$$

$$\epsilon_z = -\frac{\nu^c}{E^c} \sigma_r^c + \frac{1}{E^c} \sigma_z^c - \frac{\nu^c}{E^c} \sigma_\theta^c, \quad (38b)$$

$$\epsilon_\theta^c = -\frac{\nu^c}{E^c} \sigma_r^c - \frac{\nu^c}{E^c} \sigma_z^c + \frac{1}{E^c} \sigma_\theta^c, \quad (38c)$$

$$\gamma_{rz} = \frac{2(1 + \nu^c)}{E^c} \tau_{rz}^c. \quad (38d)$$

Equation (38d) yields

$$\tau_{rz}^c = \frac{E^c}{2(1 + \nu^c)} \gamma_{rz}. \quad (39a)$$

The remaining three equations can be rewritten in the form

$$\begin{bmatrix} -\nu^c/E^c & -\nu^c/E^c & -1 \\ 1/E^c & -\nu^c/E^c & 0 \\ -\nu^c/E^c & 1/E^c & 0 \end{bmatrix} \begin{bmatrix} \sigma_r^c \\ \sigma_z^c \\ \epsilon_\theta^c \end{bmatrix} = \begin{bmatrix} (-1/E^c)\sigma_\theta \\ \epsilon_r + (\nu^c/E^c)\sigma_\theta \\ \epsilon_z + (\nu^c/E^c)\sigma_\theta \end{bmatrix}, \quad (40)$$

with the solution

$$\sigma_r^c = \frac{E^c(\epsilon_r + \nu^c\epsilon_z) + \nu^c(1 + \nu^c)\sigma_\theta}{[1 - (\nu^c)^2]}, \quad (39b)$$

$$\sigma_z^c = \frac{E^c(\epsilon_z + \nu^c\epsilon_r) + \nu^c(1 + \nu^c)\sigma_\theta}{[1 - (\nu^c)^2]}, \quad (39c)$$

$$\epsilon_\theta^c = \frac{[1 - 3(\nu^c)^2 - 2(\nu^c)^3] \sigma_\theta/E^c - \nu^c(1 + \nu^c)(\epsilon_r + \epsilon_z)}{[1 - (\nu^c)^2]}. \quad (39d)$$

Similarly, the quantities in steel are

$$\tau_{rz}^s = \frac{E^s}{2(1 + \nu^s)} \gamma_{rz}, \quad (41a)$$

$$\sigma_r^s = \frac{E^s(\epsilon_r + \nu^s\epsilon_z) + \nu^s(1 + \nu^s)\sigma_\theta}{[1 - (\nu^s)^2]}, \quad (41b)$$

$$\sigma_z^s = \frac{E^s(\epsilon_z + \nu^s \epsilon_r) + \nu^s(1 + \nu^s)\sigma_\theta}{[1 - (\nu^s)^2]}, \quad (41c)$$

$$\epsilon_\theta^s = \frac{[1 - 3(\nu^s)^2 - 2(\nu^s)^3] \sigma_\theta / E^s - \nu^s(1 + \nu^s)(\epsilon_r + \epsilon_z)}{[1 - (\nu^s)^2]}. \quad (41d)$$

If the elastic analysis is to be followed by a time-independent inelastic analysis with say plasticity and concrete cracking, it is tempting to reuse the same mesh with smeared material constants for an axisymmetric inelastic analysis. The onset of these inelastic phenomena occurs at locations of peak values in stresses or strains. If on the basis of a cross-sectional plane stress or plane strain analysis it is concluded that the stresses and strains depend on circumferential location only locally in the smeared region S and its immediate vicinity, and if in this region it can be shown that the condition for the onset of inelastic behavior will not be met, then one can soundly proceed with an axisymmetric analysis.

Quite obviously, this smearing technique can be applied to a variety of composite structural features. Two examples that are planned for GCFR closure plug work are an array of axial hole penetrations and an orderly array of reinforcement bar aligned radially and axially.

1.4 PLANNING OF EXPERIMENTAL MODEL TESTS

A series of 1/15- and 1/4-scale model tests will be conducted on both the steam generator cavity closure plug and the reactor cavity closure plug. The relative dimensions, tolerances, and mechanical properties of the prototype will be modeled as closely as practicable. Because of the small physical size of the 1/15-scale models relative to the available miniature strain gages, the regions of highest strain gradient will not be accurately measured. Consequently, the 1/15-scale model tests will be used to demonstrate gross structural behavior and to provide the information required to maximize the effectiveness of the more costly 1/4-scale model studies.

1.4.1 Test Plan

The following procedure is being followed in conducting tests of the 1/15- and 1/4-scale closure plug models.

1. Design and fabricate model test fixture. A single 1/15-scale and a single 1/4-scale test fixture should suffice for all the tests.
2. Design and fabricate closure models.
3. Set up test facility and instrument model.
4. Conduct test. Pressure will be cycled from zero to maximum cavity pressure (MCP) ten times; pressure will then be increased monotonically until gross failure occurs or until capacity of the test fixture and/or testing facility is reached. Once the elastic range is exceeded, but the applied pressure does not exceed the design pressure, the strain in the concrete at the top and bottom of the closures and the deformations will be measured to establish that the stresses do not exceed the calculated permissible limit values. We will also ascertain whether the deformations are reversible upon reduction of pressure. The behavior of the closures will then be studied by increasing the pressure in sufficiently small steps up to ultimate failure so that the limit of deformation reversibility and the various phases of failure can be detected and the ultimate pressure capacity established.
5. Analyze data and report conclusions.

The order of testing will be: (1) 1/15-scale steam generator cavity model, (2) 1/15-scale central core cavity model, (3) 1/4-scale steam generator cavity model, and (4) 1/4-scale core cavity model, with steps 1 through 5 being repeated for each model. Depending upon available funding certain steps can be permitted to overlap chronologically.

Acceptance criteria for a plug are that the models exhibit a strength equal to or exceeding a 30.2 MPa gage (4380 psig) pressure loading (3.0 MCP), that the elastic deformations are tolerable, and that failure is progressive (gradual).

1.4.2 Design of the Test Fixture

The first test to be performed is a 1/15-scale model of the steam generator cavity closure plug shown in Fig. 1.10. Although the present

design calls for both primary and secondary hold-down systems, the first planned test deals exclusively with the structural behavior of the plug under primary hold-down.

Several conceptual designs of the 1/15-scale closure model test fixture have been examined with the objectives that the primary hold-down toggle reaction orientation and location be realistically scaled and that a test fixture pressurization capability of 138 MPa gage (20,000 psig) be attained. These objectives are premised on the viewpoint that realistic modeling should be employed and that the test fixture should have an appreciable pressurization capability in excess of anticipated model failures. No attempt will be made to duplicate support compliance; the model support must of necessity be overdesigned to accommodate the anticipated loadings associated with failure of the plug.

The space limitations imposed by geometric features of the model prohibit long toggle members as appear in the actual prototype hold-down system. An amenable arrangement that will simulate this system employs a ring of spherical balls arranged in a configuration similar to a conventional ball thrust bearing except that each ball and its spherical seat are independent from the remainder of the set. The balls in this arrangement are limited to approximately 9.5 mm (3/8 in.) in diameter. An analysis using ASME Section III Nuclear Code rules assuming use of AISI 52100 balls with a hardness of R_c 58 predicted that such an arrangement is unsatisfactory. However, a similar analysis of test data by SKF Industries, Inc. on crushing load of balls loaded in a three-ball configuration indicated that the analytical model is grossly conservative. Consequently, to verify the applicability of the conceptual arrangement, a screening test was run on 9.5 and 12.7-mm-diam (3/8- and 1/2-in.) balls, and then further verification tests were conducted with three 9.5-mm-diam (3/8-in.) balls and associated seats. In the latter tests a load of 0.22 MN (50,000 lb) (load cell limitation) was applied to the ball seat arrangements without causing failure of the balls or seats. The balls flowed plastically with deformations typically of ± 0.08 mm (± 0.003 in.). Compliance at the 0.22-MN (50,000-lb) load level

was typically 0.53 mm (0.021 in.). Hydrostatic loading was essentially achieved with the resulting average calculated uniaxial compressive stress of 3.10 GPa (450,000 psi) being well in excess of typical strength levels obtained for uniaxial specimens.

These data and the proposed conceptual arrangement were discussed at the March 25 meeting with General Atomic personnel in La Jolla, California. The adaptation of the concept to the loading fixture was approved. Subsequently layouts of the test fixture parts were initiated. The resulting test fixture system is shown in Fig. 1.25.

Fig. 1.25. Scale Steam Generator Cavity Closure Model Test Assembly Cross Section.

At this point, a plane stress analysis and an axisymmetric elastic finite-element analysis were performed on the proposed test fixture hold-down system, Fig. 1.25. The system was analyzed for 6.9 MPa (1 ksi) operating pressure with the code CREEP-PLAST.¹ The analysis was to verify that the structural integrity of the system will be maintained for overpressure loadings of up to 138 MPa (20 ksi). The analyses were interpreted in accordance with the design rules of Section III, ASME Boiler and Pressure Vessel Code, in order to meet internal ORNL requirements for pressure vessels.

The mesh shown in Fig. 1.26 consisting of 744 constant-strain triangles and 423 model points, was used for the analyses. The assumed loading conditions are a 6.9 MPa (1 ksi) pressure applied to the lower surface of the pressure ring, and a concentrated 44.00-kN (9891-lb) force inclined at 25° from the vertical and applied to the ball [44.00 kN/radian (9891 lb/radian) in the axisymmetric analysis]. This loading represents the resultant force produced by the 6.9 MPa (1 ksi) pressure acting on the plug model. The material properties used were those of the HY-100 forging being procured for the test fixture. The following important factors were considered in our development of the analytical model:

1. Plane stress vs axisymmetric solution. The solution obtained for plane stress neglects hoop stress. The shear ring is segmented with no guaranteed contact between adjacent segments; thus, plane stress is a close approximation of the state of stress in this case. The remaining system components, including the hold-down bushings, are essentially continuous axisymmetric rings. By coupling the plane stress analysis with the axisymmetric analysis, one can place bounds on the hold-down system behavior. For the immediate purpose of evaluating compliance with the ASME Code requirements, we feel that a plane stress analysis in which hoop stress is neglected and radial and axial stresses are exaggerated affords a conservative estimate.

2. Distribution of nodal points. Adjacent parts of the system share common nodal points; that is, the system is modeled as a single continuous body. Two conditions generally occur at the contact surface

Fig. 1.26. Finite-Element Mesh of 1/15-Scale Steam Generator Cavity Model Hold-Down System.

of two bodies; namely, (a) no tensile stress can be supported normal to the contact surfaces since separation of the parts would result, and (b) upon initiation of slippage, the shear stress on the contact surfaces of both bodies is equal to the product of the coefficient of friction times the normal contact stress.

Condition (a) introduces no complications since normal contact stresses at all surfaces are compressive. However, the shear stress condition does constitute an oversimplification since more load is transmitted between adjacent parts via shear stress in this analytical model than would actually occur.

3. Boundary conditions. The boundary conditions are somewhat idealized in this analytical model, as no displacement is permitted along the walls of the bushings, where in actuality they are threaded into the vessel. The nodal points along these edges are assumed by the analysis to be simply supported. Also, no friction is admitted along the outer surface of the shoe, which is located below these bushings and in contact with the vessel. These particular nodal points are on rollers free to translate axially.

The output has been examined in light of the ASME Sect. III rules. The parts were adequately proportioned to satisfy the selected design objective of 138 MPa gage (20,000 psig) at room temperature, thereby achieving a sizeable margin above the minimum pressure requirement of 3 MCP or 30.2 MPa gage (4380 psig) to ensure that the ultimate load of the closures will be determined.

1.4.3 Instrumentation and Fabrication of the Test

The system of Fig. 1.25 will shortly be under construction. The pressure loading will be applied hydrostatically. The model will be instrumented with weldable strain gages on the surface and embedment gages in the concrete. Also, deflection will be measured on the surface with displacement gages. The finite-element analyses of Sect. 1.2 are used in positioning these gages and determining approximate magnitudes of strains and displacements to be measured.

Instrumentation accessibility will be provided to pressurized surfaces of the models. Access will be through the bottom of the test fixture. Four autoclaved lead-through fittings will be provided; each permits passage of eight commercial mineral-insulated, three-wire 1.5-mm diam. (0.060 in.) stainless-steel-sheathed thermocouple leads. These fittings have been verified for 276 MPa (40,000 psi) pressures at temperatures ranging from 0 to 88°C (32–190°F).

Detailed drawings of the system have been completed. The necessary material has been procured, and a cost estimate for fabrication is in process. The 1/15-scale test will be conducted in an existing shielded test pit in Building 9201-3 of the Y-12 Plant. This facility has been previously used⁷ for testing of 0.15-m-diam (6-in.) steel vessels to pressures of approximately 240 MPa (35,000 psig).

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2. GCFR IRRADIATION EXPERIMENTS

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2.1 INTRODUCTION

A series of fuel-rod irradiation tests has been conducted at ORNL in support of fuel development for the Gas-Cooled Fast Breeder Reactor (GCFR). The GCFR is being developed at General Atomic Company (GA) with support from the Energy Research and Development Administration (ERDA) and a number of electric utility companies. The fuels-irradiation program for the GCFR consists of both thermal- and fast-flux testing in capsule and loop experiments. Thermal-flux tests are being performed at ORNL in a series of capsule tests in the Oak Ridge Research Reactor (ORR) poolside facility. These tests, supported by ERDA, represent the combined efforts of GA, ORNL, and Argonne National Laboratory (ANL).

Two fuel-rod concepts for the GCFR have been investigated in the ORNL thermal-flux tests: a sealed fuel rod and a vented-and-pressure-equalized fuel rod. Earlier tests of 20 fuel rods of the sealed type (ORR capsules P-1 through P-8) indicated this concept to be a feasible backup design; however, cladding temperatures and dimensions must be carefully specified and controlled to ensure that cladding ovality and subsequent failure by localized cladding collapse do not occur under the high coolant pressure (9 MPa) in the GCFR.

The current irradiation test, capsule GB-10, and the preceding one, capsule GB-9, were designed to permit an initial evaluation of the overall performance of the vented-and-pressure-equalized fuel rod, which is the reference fuel-rod design for the GCFR. Many important aspects of the GCFR pressure-equalization system¹ are also being studied in these tests. The GB-9 and GB-10 experiments have provided substantial information²⁻¹³ on the fission-product-release and fuel behavior to be expected from the GCFR vented fuel rod. The experimental data have been used in many aspects of the GCFR pressure equalization system design and in computer models for predicting fast breeder reactor fuel performance.

Postirradiation examination of the GB-9 fuel rod, which was irradiated at 48.6 kW/m (14.8 kW/ft) peak power to a fuel burnup of approximately 54 MWd/kg-heavy metal, has been completed at ANL and GAC.^{7,13}

The GB-10 fuel rod, similar to GB-9 but designed with greater capability for measuring fission product release and transport, is being irradiated in three successive peak power steps: 39.4, 44.3, and 48.6 kW/m (12, 13.5, and 14.8 kW/ft) at peak cladding outer surface temperatures of 565, 630, and 685°C, respectively. The estimated burnup of the GB-10 fuel rod on June 25, 1975, (708 days irradiation) was 70 MWd/kg heavy metal. The original burnup goal of 75 MWd/kg has been extended to 100 MWd/kg to permit additional experimental measurements.

The GB-10 measurements are a continuation of the measurements started with capsule GB-9. The GB-9 capsule, being the first of a kind, yielded a large amount of information, particularly for the normal operating conditions of a GCFR fuel rod. The GB-10 capsule, with its additional sweep lines, permits experiments of greater depth involving both normal and off-normal conditions. The main objective of GB-10 is to obtain detailed information on the release, transport, and trapping of gaseous and volatile fission products. Another objective is to observe the general performance of the rod and to look for any behavior that could be a potential problem for the GCFR.

Capsule GB-10 has provided, for the first time, direct measurements of the release of gaseous and volatile fission products from the solid matrix of an operating fast breeder reactor fuel rod. Another notable accomplishment of GB-10 has been the measurement of flow conductance through the fuel rod, while operating at power, as a function of burnup.

Because of the need for additional experimental data that only GB-10 could provide at the present time, the GB-10 measurements and goals were expanded during the course of the irradiation. During the current report period, three systems were installed to increase GB-10 capability for study of fission product release and transport. These were (1) an on-line Ge(Li) gamma-ray detector system, which permits easier and more detailed fission gas release measurements; (2) a system for taking large ($\approx$ 1-liter) gas samples for the determination of stable noble-gas

release rates using mass spectrometry; and (3) a system for semicontinuous measurement of the tritium content in the effluent sweep stream.

2.2 DESCRIPTION OF CAPSULE GB-10

The GB-10 fuel rod (Fig. 2.1) is a shortened prototype of the GCFR rod with a 0.23-m-long (9-in.) region of sol-gel-derived (U,Pu)O₂ solid pellet fuel, a 51-mm-long (2-in.) upper blanket region of depleted UO₂ pellets, and a 25.4-mm-long (1-in.) charcoal trap. The two UO₂ half-pellets at each end of the mixed-oxide column were included to suppress power peaking at the ends of the test fuel. Initial oxygen-to-metal ratios (O/M) were 1.98 in the (U,Pu)O₂ pellets, 2.004 in the UO₂ half-pellets, and 2.002 in the depleted UO₂ blanket pellets. Fuel column smear density was 84.2% of theoretical. The charcoal trap contains 0.461 g of activated coconut charcoal, type MI-6736. The cladding is 20%-cold-worked stainless steel with a roughened outer surface.

The fuel rod is shown inside the instrumented GB-10 capsule in Fig. 2.2. The NaK-filled capsule is equipped with an electrical heater formed into a coil over the upper blanket and trap regions of the fuel rod. The charcoal trap temperature can be controlled over the range 200 to 400°C by adjusting the output of the heater. The fuel region power level is controlled by adjusting the capsule position in the ORR poolside facility (i.e., adjusting the distance from the reactor core). Thirteen thermocouples monitor cladding temperatures along the rod. Fuel region power profiles are estimated from the thermocouple indications by use of heat transfer equations. Additional details of the capsule and fuel rod design were reported previously.^{10,11}

The GB-10 arrangement of sweep lines and valves (Fig. 2.3) permits flow across the top of the fuel rod, which is the normal sweep flow mode, or flow through the three main regions of the rod (trap, blanket, and fuel), either individually or in combination. As shown in Fig. 2.3, we have designated the sweep lines as follows: BF = line to bottom of fuel, TT = lines to top of charcoal trap, BT = line to bottom of trap, and BB = line to bottom of upper blanket. Specific sweep flow modes are designated by two letters indicating the entrance point followed by two

ALL DIMENSIONS IN INCHES

Fig. 2.1. Fuel Rod Specimen for Irradiation in GCFR-ORR Capsule GB-10. To convert dimensions to millimeters, multiply by 25.4.

Fig. 2.2. GCFR-ORR Capsule GB-10. Height indicated is 392.7 mm.

Fig. 2.3. Schematic of GCFR-ORR Capsule GB-10, Showing Sweep Gas Lines.

more letters indicating the exit point from the fuel rod. Thus, when the sweep flow is directed across the top of the rod, the flow mode is TT-TT and when the sweep flow is directed into the bottom and out of the top of the rod the flow mode is BF-TT. The latter flow mode (BF-TT) simulates a leak in the cladding of a GCFR fuel rod.

Two of the lines shown in Fig. 2.3, the one with the pressure regulating valve and the capsule bypass line, were recently added to the system to aid in making fission-gas release measurements at very low flow rates through the fuel region of the rod.

The sweep and cladding-external gas systems for capsule GB-10 are shown in Fig. 2.4. Flow through the sweep system (0.15-1.3 std liters/min) is regulated by adjusting the downstream flow resistance with a needle valve. Flow restrictors are employed immediately upstream of the needle valve to limit the flow rate out of the system to a maximum value. An automatic pressure differential control valve adjusts the inlet sweep flow and maintains the sweep pressure 172.4 ± 0.7 kPa (25 ± 0.1 psi) above the pressure in the normally static cladding-external gas system. Analyzed helium is used as the sweep gas and is passed through room-temperature molecular-sieve traps and then through 625°C titanium-sponge traps before going to the capsule.

At the start of the GB-10 irradiation, fission-gas release was monitored by two on-line ionization chambers and by gamma-ray spectrometry of gas samples taken periodically. The on-line ionization chambers, one located on the high-pressure section of the effluent sweep line and the other on the low-pressure section, continuously monitor the gross activity of the sweep line and provide a sensitive indication of the steadiness of fission gas release from the rod. Since the start of the GB-10 irradiation, three additional fission-product monitoring systems have been designed and installed. An on-line Ge(Li) gamma-ray detector system (see Fig. 2.4) was added to the high-pressure section of the effluent sweep system to permit easier and more detailed fission gas release measurements. The other two systems, both added to the low-pressure section of the effluent sweep system, are one for taking large gas samples for determination of stable krypton and xenon release rates and one for monitoring tritium.

Fig. 2.4. GCFR-ORR Capsule GB-10 Sweep and Cladding-External-Gas Systems.

The GB-10 on-line gamma-ray spectrometer system consists of a 6.35-mm-ID (0.25-in.) source section in the effluent sweep line, five 0.305-m-long (12-in.) stainless-steel-lined lead collimators ranging from 1.6 to 32 mm ID (1/16-1 1/4 in.) to cover the wide activity range associated with the various GB-10 flow modes, a Ge(Li) detector (8.6% efficiency for ^{60}Co) with associated Dewar and power supply, preamplifier located at the detector, main amplifier located at the analyzer, and a Nuclear Data 50/50 data acquisition system (PDP-15 computer interfaced with a 4096-channel analyzer). The separation distance between the source section and the detector (center-line-to-center-line) is 0.597 m (23 1/2 in.). Before installation, the detector system was calibrated with each of the collimators in place with an exact mockup of the actual experimental setup and calibrated radionuclide sources.

A schematic of the system for taking large samples for stable noble gas (SNG) release determination is shown in Fig. 2.5. In taking these samples, we direct the effluent sweep flow through the shielded 2-liter vessel inside the shielded valve box for an appropriate length of time

Fig. 2.5. Schematic of Capsule GB-10 Sampling System for Stable Noble Gas Release Determination.

(about 10 volume changes) and then trap a sample of the gas in the vessel. The sample is allowed to decay for 6 weeks, and then about half of it (≈ 1 std liter of gas) is drawn off into a clean 2-liter sample vessel outside the valve box. The SNG sample is then removed and prepared for analysis by mass spectrometry. Special techniques are required to concentrate the krypton and xenon isotopes to measurable levels.

The GB-10 tritium monitoring system (Fig. 2.6) is designed to measure the tritium (T) concentration in the effluent sweep gas under various capsule operating conditions and sweep flow modes. In addition, provision is made for introducing HT-in-He calibration gases (at four different tritium concentrations), either directly to the tritium monitoring system for calibration checks or to the GB-10 inlet sweep system for tritium transport experiments. The same gas supply manifold may be used to introduce other premixed gases to the inlet sweep system when desirable.

The main components of the tritium monitoring system are a HTO-to-HT converter (magnesium at 500°C), a liquid-nitrogen-cooled charcoal trap

Fig. 2.6. Schematic of Capsule GB-10 Tritium Monitoring System.

assembly (charcoal at -188°C), a calibrated flow-through ionization chamber and vibrating-reed electrometer, an HT-to-HTO converter (CuO at 500°C), and a removable HTO trap assembly (molecular sieve). Sweep gas from the capsule flows through the charcoal trap designed to delay all radioactive gases but tritium for long periods relative to the time required for the measurements. Tritium content in the gas stream leaving the charcoal trap is then determined by observing the response of the calibrated ion chamber and by batchwise collection and analysis of tritium using the removable HTO traps and beta liquid scintillation counting methods. The batchwise sampling method is used as needed to verify and/or supplement the ion chamber data. The HTO-to-HT converter upstream of the charcoal trap enables measurement of total tritium when it is valved in and only gaseous tritium when it is valved out, since the charcoal will trap the moisture form. The charcoal trap was performance-tested and the ion chamber was calibrated before installation in the system. The other main components were simulated in laboratory-scale experiments to verify their design.

Tritium monitoring experiments are planned to obtain information on tritium release rates from the mixed-oxide fuel pellets, relative cladding-permeation and venting rates from the rod fuel, molecular form (HT and/or HTO) of the released tritium, and the effect of low levels of H_2 and H_2O in the sweep gas on tritium transport behavior in the fuel-fission-product-helium environment. To fully understand tritium transport behavior in the CB-10 system may require injecting H_2 and H_2O into the inlet sweep gas to determine their influence on tritium transport. Before any injections of this type are made, however, the existing levels of H_2 and H_2O in the normal sweep gas are to be determined, and the effect on fuel and fission product chemistry of any planned changes in impurity levels is to be evaluated.

Instruments for determining the H_2 and H_2O contents in the sweep gas upstream and downstream of the capsule are currently being evaluated and calibrated in laboratory tests. A refrigerated moisture generator is also being tested in the same setup to determine its feasibility for adding H_2O to an inlet sweep gas of known H_2 content and to control the

H₂O/H₂ ratio in the gas. Preparation of the GB-10 system for installation of these components has started.

2.3 IRRADIATION CONDITIONS FOR CAPSULE GB-10

The irradiation of capsule GB-10 in the ORR poolside facility was started in August 1972, at a fuel-rod peak power level of 39.4 kW/m (12 kW/ft) (565°C peak cladding outer surface temperature). Operation at 39.4 kW/m was continued to an estimated fuel burnup of 27.1 MW/kg heavy metal. The fuel-rod power was then raised to 44.3 kW/m (13.5 kW/ft) (630°C peak cladding outer surface temperature), where operation has continued satisfactorily to the present burnup level of 70 MW/kg on June 25, 1975. Following completion of planned experiments at 44.3 kW/m, the power will be increased to 48.6 kW/m (14.8 kW/ft) (685°C peak cladding outer surface temperature) for the remainder of the irradiation. The fuel burnup goal has been revised from its original value of 70 to 100 MW/kg.

Operating conditions for the fuel rod are listed in Table 2.1. These conditions are typical of those expected for a GCFR fuel rod except for the absence of significant fast-neutron exposure. Sweep flow is maintained whenever the capsule is at power, so that fission gases may be free to diffuse out of the fuel rod as in the case of the GCFR rod. The irradiation is interrupted occasionally for short periods for reactor refueling shutdowns (a few hours once every two weeks) and reactor end-of-cycle shutdowns (a few days every two months). Other interruptions of a few weeks have occurred for installation of new equipment and maintenance work.

Table 2.1. Steady-State Operating Conditions of GCFR-ORR Capsule GB-10

Fuel-Rod Operating Power Levels, kW/m (kW/ft)	39 (12.0)	44.3 (13.5)	48.6 (14.8)
Cladding Outer Surface Temperature, °C			
Fuel Region, Peak	565	630	685
Charcoal Trap	500	300	300
Cladding Pressure, MPa gage (psig)			
External			6.7 (975)
Internal			6.9 (1000)
Fuel Burnup Goal, MW/kg of Heavy Metal			100

2.4 RESULTS FROM IRRADIATION TESTING OF CAPSULE GB-10

The GB-10 experimental data obtained to date have included (1) measurements of the fuel-rod flow resistance as a function of fuel burnup, (2) measurements of steady-state fission gas release as a function of burnup, fuel-rod power level, and operating pressure, (3) measurements of fission gas release during shutdowns and start-ups, (4) measurements of volatile fission product deposition in the system, and (5) neutron radiography of the capsule before irradiation and after operation at 39.4 kW/m (12 kW/ft). The results obtained during the first 35 MMd/kg heavy metal burnup, including many of the measurements listed above, have been reported previously.^{10,12} Therefore, only the more recent results will be presented in the following sections, except where some of the earlier results are added for completeness.

During the current report period, in which the fuel burnup advanced from 35 to 70 MMd/kg-heavy metal, significant new information was obtained from the fuel-rod flow resistance and fission-gas release measurements. We consider the flow resistance data important because it indicates that plugging may be possible in a breeder reactor fuel rod. Fission gas release data were obtained as a function of burnup and operating pressure for the radioactive gamma-emitting gases. Progress in the measurement of stable krypton and xenon release rates and initial operation of the tritium monitoring system are also described in the following sections.

2.4.1 Fuel-Rod Flow Resistance vs Burnup

An indication of fuel-rod flow resistance is obtained whenever the sweep flow is directed through the fuel region. The two pressure elements shown in Fig. 2.3 provide a measurement of the pressure drop (ΔP) across the BF inlet line and the fuel rod whenever the inlet sweep flow is changed from the normally used IT line to the BF line. Flow resistance can then be estimated from the flow rate and pressure drop indications. Pressure drop calculations indicate that about 21 m (70 ft) of 0.69-mm-ID (0.027-in.) BF inlet sweep line accounts for about 34 kPa (4-5 psi) of the total measured ΔP for the conditions of 6.9 MPa gage (1000 psig) pressure and 1.0 std liter/min flow rate. Thus, ΔP

measurements that are much greater than 34 kPa (5 psi) indicate fuel-rod resistance to flow and/or possible plugging of the BF inlet sweep line. We believe the past and recent trends in the flow resistance data are due to changes in the fuel rod resistance rather than to plugging of the BF line.

The recent trend in the BF-TT flow resistance at 44.3 kW/m (13.5 kW/ft) operation is shown in Fig. 2.7, where the measured flow and ΔP data were reduced to expected flow rates at a pressure differential of 1.72 MPa (250 psi), Q_{250} , and plotted against irradiation time and estimated fuel burnup. An arbitrary operational limit of 1.72 MPa was placed on the pressure differential for fear that higher differentials might redistribute the hot fuel.

We normally prefer to make release measurements at a sweep flow rate of 1.2 std liters/min, which gets the released gases to the

Fig. 2.7. Variation of GB-10 Flow Rates in the BF-TT Mode with Irradiation Time.

monitoring points in less than a minute. During the first half of the irradiation, this flow rate could be achieved easily. However, with higher burnup and accumulation of fission products, an exponential decline in the normalized BF-TT flow rate occurred, as shown in Fig. 2.7, until now we can flow only about 20 std cm^3/min through the fuel rod at the 44.3 kW/m fuel-rod power level. When corrected to the operating pressure of 6.9 MPa gage (1000 psig), 20 std cm^3/min is only about 0.3 cm^3/min actual flow rate. At flow rates of 20 std cm^3/min or below, we have measurement difficulties as well as extremely long stabilization and response times following a flow rate adjustment.

We have found that a shutdown or a reduction in power level opens up the BF-TT flow passages. However, upon return to power following a short shutdown, the flow resistance returns to about its previous level. When the capsule was shut down for two months (at ~69 MWd/kg fuel burnup) for installation of the tritium monitoring system, we found on return to power that the flow resistance had almost disappeared, but two weeks later when we next attempted flow through the rod, the restriction had returned (see Fig. 2.7).

Although the BF-TT flow resistance at 44.3 kW/m has increased sharply at high burnup, the flow restriction is still far from a condition that could cause a complete flow blockage and fission-gas pressure buildup within a GCFR rod. The GB-10 data do indicate a tendency for fuel rod plugging in the particular fuel-fission-product-helium sweep environment of the test; therefore, additional work in this area may be needed to ensure that plugging does not occur under GCFR conditions.

2.4.2 Steady-State Fission-Gas Release Rates Versus Burnup and Operating Pressure

Steady-state fission-gas release rates for the radioactive gamma-emitting gases have been measured as a function of time at fuel-rod power levels of 39.4 and 44.3 kW/m (12 and 13.5 kW/ft) under all the planned sweep flow modes. During the first year of operation, we routinely made sets of measurements under the TT-TT, TT-BT, TT-BB, and BF-BB flow modes (see Fig. 2.3 and associated text for flow mode definitions). From these data, transport delay times through each main region

of the rod were determined for various fission gas isotopes. After the first year, we started experiencing a partial blockage of the BB line and discontinued its routine use.

Typical fractional release data (ratio of measured release rate to calculated birth rate, R/B) obtained during the early part of the irradiation are shown in Fig. 2.8 for the TT-TT and BF-BB flow modes. All the release data during this time were obtained by taking gas samples, some of which were analyzed with a Ge(Li) detector, "primed points," and the others with an NaI detector, "unprimed" points. With this method of measurement, we obtained release data for isotopes with half-lives ranging from 3.18-min ^{89}Kr to 5.27-day ^{133}Xe .

The initial release rates from the mixed-oxide fuel [Fig. 2.8(b)] were relatively high, but they decreased, turned around, and then continued a gradual increase with burnup during operation at 39.4 kW/m. This behavior is believed to be associated with fuel temperature changes caused by fuel restructuring and by changes in the fuel-cladding gap and fuel thermal conductance. At any given time, release is extremely sensitive to temperature.

After the fission product gases are released from the solid oxide-fuel matrix, their transport and venting from the top of the rod are controlled during steady-state operation by their diffusion through the high-pressure helium. Therefore, in taking the samples under steady-state operation, the normal sampling order is to start at the top of the rod (TT-TT sweep flow mode) and work downward so that the inventory of fission gases below the sweeping point is not upset.

The release rates for the TT-TT flow mode [Fig. 2.8(a)], or for the fission gases normally vented from the rod, show the same trends as the release rates from the mixed-oxide fuel [Fig. 2.8(b)], and the release rate of the long-half-life ^{133}Xe is about the same for both flow modes, as expected. The spread as a function of half-life is much greater for the vented gases, reflecting the gas-phase transport delay times.

A continuation of the venting (TT-TT flow mode) and fuel release data through most of the current reporting period is shown in Fig. 2.9. At a burnup of approximately 25 MWd/kg, we started using the BF-TT flow

- 1 "PINNED" DATA POINTS ARE RESULTS OF SAMPLES COUNTED WITH A 30-cm³ GEL-1 DETECTOR (4.4% EFFICIENCY FOR ⁸⁵Kr) AND 4096-CHANNEL ANALYZER SYSTEM
- 2 "UNPINNED" DATA POINTS ARE RESULTS OF SAMPLES COUNTED WITH A 3x3-in. NaI DETECTOR AND 512-CHANNEL ANALYZER

Fig. 2.8. Steady-State Fission-Gas Release from GB-10 Fuel Rod vs Time. (a) Sweep flow mode TT-TT. (b) Mode BF-BB.

1. PRIME DATA POINTS ARE RESULTS OF SAMPLES COUNTED WITH A 30 cm³ GELLY DETECTOR 4.4% EFFICIENCY FOR 60Co AND 4096-CHANNEL ANALYZER SYSTEM
2. UNPRIME DATA POINTS ARE RESULTS OF SAMPLES COUNTED WITH A 3 x 3-in NaI DETECTOR AND 512-CHANNEL ANALYZER
3. DATA POINTS IDENTIFIED WITH PLUS SIGN ARE RESULTS OF ON-LINE ANALYSES MADE WITH THE GB-10 ON-LINE SYSTEM GELLY DETECTOR 19.6% EFFICIENCY FOR 60Co AND A 4096-CHANNEL ANALYZER SYSTEM

- (b)
- * FLOW MODES ARE DESIGNATED BY TWO LETTERS INDICATING THE ENTRANCE POINT FOLLOWED BY TWO MORE LETTERS INDICATING THE EXIT POINT FROM THE FUEL ROD
 BF = BOTTOM OF FUEL, TT = TOP OF CHARCOAL TRAP, BT = BOTTOM OF CHARCOAL TRAP, BB = BOTTOM OF UPPER BLANKET

Fig. 2.9. Steady-State Fission Gas Release from GB-10 Fuel Rod vs Time. (a) Sweep flow mode TT-TT.* (b) Mode BF-TT.

mode [Fig. 9(b)] on a routine basis in place of the BF-BB flow mode, since a partial flow blockage of the BB line was developing. With respect to release of the fission gases, we have not seen any significant differences using any of the three fuel sweep modes, BF-BB, BF-BT, or BF-TT.

When the fuel-rod power was increased from 39.4 to 44.3 kW/m, very high release rates from the fuel were observed initially [see Fig. 2.9(b)]. Apparently part of the higher fission-gas inventory in the mixed oxide fuel at the lower temperature and power level was released when the temperature and power were increased, causing the temporary excessive release fractions.

After the release rates had stabilized following the power increase to 44.3 kW/m, we started operation at reduced sweep pressure to determine the effect of operating pressure on steady-state fission-gas release. The capsule was operated at a reduced sweep pressure of 1.48 MPa (200 psig) for two months (5.0 MWd/kg burnup) and then at 2.17 MPa (300 psig) for the same length of time. The pressure effect on release from the fuel [Fig. 2.9(b)] was greater than expected. Apparently helium pressure affects release from the fuel, and after each large change in the pressure level the new equilibrium takes a long time to establish. After return to 7.0 MPa (1000 psig) the release rates returned to a level consistent with that before the reduced pressure operation.

The GB-10 on-line Ge(Li) detector system was placed in operation in March 1974, when the estimated fuel burnup was about 41 MWd/kg. Data points obtained with the on-line system are identified with a "plus" sign in Fig. 2.9. Using the on-line system eliminates sample handling and lets us measure isotopes with shorter half-lives than is possible by the gas sampling method. From some spectra, we determined release rates of 10-sec ^{91}Kr and 16-sec ^{140}Xe [see Fig. 2.9(b)]. Also, the measurements are much easier with the on-line system. Although not apparent in the figures, gas samples containing less than 0.01 std cm³ of the BF-TT gas typically read 1 R/hr or more on contact.

The release rates for the TT-TT flow mode, or venting rates, during the reduced-pressure operation [Fig. 2.9(a)] showed the same release patterns as the fuel release rates showed and also the expected effect of pressure on gas-phase transport, the effect being greater the shorter the half-life of the isotope. For example, the venting rates of 15.6-min ^{135}mXe and 17-min ^{136}Xe were more than a factor of 10 higher at 1.48 MPa (200 psig) than at 7.0 MPa (1000 psig). These data show clearly how the high operating pressure suppresses steady-state venting of the fission gases. In fact, we had originally planned to go to 0.79 MPa (100 psig) following the 1.48 MPa (200 psig) operation, but we decided to obtain the release data at the intermediate pressure of 2.17 MPa (300 psig) instead because of the high activity release at low pressure.

In all the GB-10 fractional release (R/B) data reported to date, we have calculated the isotopic birth rates (B) on the basis of the initial loading of fissile atoms. Initially the principal fissile isotopes were ^{239}Pu and ^{235}U . We have assumed in the birth rate calculations that no change has occurred in the fraction of fissions due to ^{239}Pu and ^{235}U from their initial values of 0.664 and 0.336, respectively, and we have not taken into account buildup of ^{241}Pu . These assumptions are important since the yields per fission from ^{239}Pu , ^{241}Pu , and ^{235}U are not the same for many of the fission-product isotopes. The total fission rate used in the birth rate calculations is based on the fuel-rod linear power distribution at the time of the release rate (R) measurement, which in turn is estimated from temperature indications with heat transfer equations.

A recent burnup calculation for GB-10 was made to determine nuclide concentrations with time and neutron flux requirements for maintaining the desired fuel-rod power levels during the remainder of the irradiation. From those results, relative fission rates for ^{239}Pu , ^{241}Pu , and ^{235}U were estimated at several different irradiation times to determine the magnitude of error involved in the present GB-10 birth rate calculations, where changes in the relative fission rates are neglected. The results of these calculations are given in Table 2.2. The 0.664 and 0.336 being used for the fractions of fissions due to ^{239}Pu and ^{235}U in the birth

Table 2.2. Calculated Isotopic Fission Rate Distributions in Capsule GB-10

Accumulated Irradiation Time (days)	Relative Isotopic Fission Rates		
	^{239}Pu	^{241}Pu	^{235}U
0	0.668	0	0.332
600	0.555	0.046	0.399
879	0.436	0.152	0.412

rate calculations are close to the values on day 0 in Table 2.2. However, as the fuel burnup reaches high levels the relative isotopic fission rates change appreciably and should be taken into account in the calculations. We decided not to make this correction to the R/B data at this time, since we plan to make a review of all possible errors before the R/B data are reported in final form.

2.4.3 Stable Noble Gas Release Measurements at 13.5 kW/ft

The sampling system (Fig. 2.5) for taking large gas samples (~1 liter) for the determination of stable noble-gas release rates using mass spectrometry was placed in operation in June 1974. Subsequently, five samples were taken at about two-month intervals between samples. Preliminary release rate to birth rate (R/B) results for the five samples are given in Table 2.3. Some of the krypton results are close to that expected — that is, greater than 30% fractional release — but the xenon results are all too low by almost a factor of 10. Therefore, it is likely that most of these first data will be discarded later when the sampling and analysis techniques are perfected.

After the R/B results were calculated for the first sample, we began examining the special techniques being used to analyze the samples. In preparing the samples for analysis by mass spectrometry, the sample gas is spiked with ^{38}Ar and then concentrated to bring the krypton and xenon isotopes up to measurable levels. Several steps in the sample transfer and preparation process were varied in an attempt to locate a

Table 2.3. Preliminary Results of GB-10 Stable Noble Gas Release Measurements at 44.3 kW/m (13.5 kW/ft)

Date	Sweep Flow Mode	Ratio of Fission-Gas Release Rate to Birth Rate, %							
		⁸³ Kr	⁸⁴ Kr	⁸⁵ Kr	⁸⁶ Kr	¹³¹ Xe	¹³² Xe	¹³⁴ Xe	¹³⁶ Xe
7-1-74	TT-TT	8.34	11.0	8.89	10.3	1.56	1.67	1.38	2.52
10-1-74	BF-TT	33.2	51.3	61.3	42.4	3.07	3.52	3.39	6.11
11-21-74	BF-TT	23.2	23.8	21.8	21.8	4.14	4.52	4.81	8.64
1-28-75	TT-TT	14.2	15.0	14.0	14.6	0.99	1.06	1.01	1.91
3-25-75	TT-TT	24.6	25.9	23.4	25.0	2.94	3.23	3.00	5.53

problem, but as seen in Table 2.3 these efforts did not significantly improve the results. Next we took a hard look at the way we were taking the samples. After trapping a sample of the GB-10 effluent gas in the shielded 2-liter vessel inside the valve box and waiting six weeks for decay of the radioactive gases, we were evacuating the lines and then slowly opening a valve to allow the gas to pass through about 3 m (10 ft) of 3.2-mm-OD (1/8-in.) line to an evacuated 2-liter sample vessel outside the valve box (see Fig. 2.5). When the pressure in the two vessels had equalized, we closed the valves and removed the sample vessel. Following discussions with others, we now believe we may have been fractionating the gas mixture by this method of sampling, tending to leave the heavier isotopes, especially the xenons, in the first vessel.

After the results were obtained for the fifth sample, we decided to discontinue sampling until the sampling system could be modified. We are currently modifying the system to permit circulation and mixing of the gases in the two vessels before the sample vessel is isolated and removed.

2.4.4 Initial Operation of the GB-10 Tritium Monitoring System

Funding for the GB-10 tritium monitoring system was provided in late November 1974. By the middle of June 1975, the complex system had been designed, fabricated, performance-checked, installed, and trial-run operated with clean helium. A "zero base-line" reading was obtained on

the ion chamber (see Fig. 2.6) under helium flow before any tritium was introduced into the system. This reading, 0.45 mV, was about the same as that in the laboratory performance tests before installation at the reactor.

Two more tritium monitoring system runs were made during the current report period. Both these runs were system calibration checks using the lowest concentration tritium-in-helium calibration gas (5×10^{-5} $\mu\text{Ci}/\text{cm}^3$ STP). The results were encouraging in that both the response of the calibrated ion chamber [about 54 mV at 70 kPa gage (10 psig) operating pressure] and the results for tritium collected in ETO trap samples indicated the tritium concentration in the gas stream to be reasonably close to the known concentration in the supply cylinder. Also, we were able to clean up the system in about 3 hr to an ion chamber reading within a factor of 2 of the "zero base-line" value (i.e., to a base-line reading below 1 mV).

At this point it appears that the tritium monitoring system is operating as designed and that its sensitivity for measuring tritium in the capsule gas will be satisfactory.

2.5 PLANS FOR REMAINDER OF CAPSULE GB-10 OPERATION

We plan to continue the routine acquisition of experimental data at the present 44.3 kW/m (13.5 kW/ft) fuel-rod power level and at the final power level of 48.6 kW/m (14.8 kW/ft). In addition, special experiments are planned. First priority in the next few weeks will be to conduct several tritium monitoring experiments at the present power level of 44.3 kW/m to obtain an early first set of measurements of the tritium content to be expected in the GB-10 effluent sweep gas and to determine the operating characteristics of the system under these conditions. The results of these experiments will provide the information needed for planning additional experiments and perhaps will indicate to some degree the effort and number of experiments that will be required to obtain the desired tritium release and transport information. Since GB-10 has only one year of operation left, it is important to get this work started as soon as possible. Also of high priority is completion of the calibration and installation of the instrumentation for measuring the H_2 and H_2O impurity levels in the sweep gas.

After the initial tritium experiments are completed, we plan to obtain iodine deposition measurements during a shutdown period and then increase the fuel rod power to 48.6 kW/m for most of the remainder of the irradiation. After a period of operation at 48.6 kW/m is achieved, several special tests will be conducted. Near the end of the irradiation, we plan to conduct power cycling tests between 44.3 and 30 to 40 kW/m (13.5 and 9-12 kW/ft) to obtain release data and determine the numbers of cycles to reach equilibrium release conditions. In another special test we plan to measure the release spectrum as a function of sweep flow rate over a wide flow rate range. This test simulates leak rates in a GCFR fuel rod and will provide data that can be used in the design of instrumentation for locating leaking GCFR fuel elements. Some pressure cycling tests may also be conducted.

Neutron radiography of the GB-10 capsule following operation at 44.3 and 48.6 kW/m will not be possible. The ORR radiography rig, which was used to obtain the preirradiation and 39.4 kW/m neutron radiographs developed a leak and had to be removed. Replacement with a new rig is uncertain at this time.

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3. GCFR CORE FLOW TEST LOOP

A. G. Grindell

3.1 INTRODUCTION - A. G. Grindell

3.1.1 Objectives

The viability of the Gas-Cooled Fast Breeder Reactor (GCFR) depends strongly upon the steady-state characteristics and the transient responses of the core elements. Information on heat transfer and pressure drop characteristics of the GCFR core during steady-state operation is important to the design and costs of the GCFR. Information on the responses of the core to normal, upset, emergency, and accident transients anticipated for the GCFR is important to determining the design and safety margins of the core.

The Core Flow Test Loop (CFTL), which is part of the GCFR Program at ORNL, will be designed to provide this information. It is a high-pressure, high-temperature, out-of-reactor, circulating helium system that will provide the steady-state requirements of power, heat removal, and coolant helium flow rate, pressure, and temperature; the power and coolant helium requirements to simulate anticipated transients for GCFR core elements; and the rapid helium depressurization to simulate the GCFR design basis accident. In addition, the loop will provide for testing mockups of in-reactor test configurations as required.

In the CFTL, full-size blanket elements and segments of fuel and control rod elements for the GCFR core, which will be simulated with electric heaters referred to as fuel pin simulators (FPS), maintained in a specified arrangement in a test bundle, will be subjected to steady-state design operating conditions and to normal, upset, emergency, and accident transients. Heat transfer and pressure drop characteristics will be measured. Thermal and mechanical interactions between the FPSs and the grid spacers and hexagonal duct, caused principally by the transient operations and core undercooling, will be determined with both visual observation and interim and post-test measurements.

Steady-state operation of test bundles in the CFTL, which will include flat power distribution across test bundles at any power level from 100 to 5% of design and skewed power distribution with slope variable from 0.2 to 0.4% per radial millimeter (5-10%/in.) across the bundle, will be conducted to:

1. verify heat transfer and fluid-flow data obtained from other experimental programs;
2. verify analytical methods of extrapolating heat transfer and fluid-flow data to full-size core elements;
3. examine the rod bowing caused by thermal gradients across test bundles;
4. obtain vibrational data;
5. explore local flow blockage to detect blockage, to measure thermal and hydraulic effects of blockage, and to verify analytical models used to calculate effects of blockage;
6. assist the design of in-reactor test bundles and provide comparison between in-reactor and out-of-reactor bundles, as required.

Transient operation of the CFTL will include prescribed variations with time in (1) test bundle coolant helium flow rate; and (2) power from an initial flat or skewed power distribution to simulate normal, upset, and accident transients; and (3) helium pressure to simulate the design basis depressurization accident. Such operation will be conducted to:

1. verify the ability of GCFR core elements to withstand the transients;
2. determine design and safety margins of the core elements to extreme undercooling, overcooling, and depressurization transients;
3. explore the operation of core elements for structure-related behavior and deficiencies;
4. obtain ancillary information on the interaction between fuel rod and spacer grid and vibration and performance data on the outlet gas temperature sensors, the flow control orifices, and the control rods.

3.1.2 Test Capability

The CFTL will provide a total of 4100 kW of electric power to the test bundle FPSs in discrete control zones. Each zone will correspond to a row of simulators, and each row is parallel to a selected

flat face in the hexagonal test bundle configuration. In a flat power distribution an identical amount of electric power will be supplied to each simulator in a test bundle. In a skewed power distribution, although the same value of power is supplied to each simulator in a single zone, the values of total power in the several zones are varied systematically to provide a relatively smooth linear power distribution across the test bundle from a maximum at one hexagonal face to a minimum at the opposite hexagonal face. During a power transient simulating a reactor scram, the test bundle power will be reduced from 100 to 10% in 1, 2, 4, or 16 sec. Figure 3.1 indicates a 1-sec transient of GCFR core power during a reactor trip.

Fig. 3.1. Core Power versus Time During a GCFR Reactor Trip. Gravity drop of primary rods.

The CFTL will provide coolant helium to the test bundle at 8.62 to 10.34 MPa (1250-1500 psia) inlet pressure and 260 to 343°C (500-650°F) inlet temperature at mass flow rates up to 2.76 kg/sec (6.1 lb/sec), which is sufficient to remove heat from a 91-heater test bundle. During a flow transient, the coolant helium flow rate will be reduced from 100 to 10% in time periods of 1, 2, 4, or 16 sec. Figure 3.2 shows a typical coolant helium flow transient during a GCFR reactor scram.

Fig. 3.2. Flow Rate of Core Coolant Helium versus Time During a GCFR Reactor Trip.

The CFTL will provide depressurization of the coolant helium from 10.34 to 0.15 MPa (100 to 1.5 atm) absolute during time periods of 1, 2, 4, or 8 min. Figure 3.3 shows the calculated helium pressure during a 4-min depressurization as anticipated for the GCFR.

The CFTL will also permit operating a specifically designed test bundle in an approach to melting the FPS cladding. The CFTL will be equipped with a fast scan data acquisition system (FSDAS) to record real time data on magnetic tape during both fast transient and steady-state operations. We plan a high-speed 640-channel system able to take 20,000 samples of data per second. Table 3.1 presents additional steady-state operating requirements for the CFTL.

Fig. 3.3. Coolant Helium Pressure versus Time During a GCFR Design Basis Accident Depressurization. A pressure of 1 MPa corresponds to 145 psi.

Table 3.1. Steady-State Operating Requirements
in the CFTL

Coolant	Helium
Pressure, MPa (psia)	
Loop maximum	10.34 (1500)
GCFR	9.03 (1310)
Test section	
$\Delta P/P$	0.034
Inlet temperature, °C (°F)	
Nominal	321 (610)
Minimum	260 (500)
Maximum	343 (650)
Outlet temperature, °C (°F)	
Nominal	549 (1020)
Minimum	260 (500)
Maximum	593 (1100)
Helium flow per rod g/sec (lb/hr)	
Core maximum	30.5 (242)
Blanket maximum	4.4 (35)
Core maximum linear power rating, kW/m (kW/ft)	54.1 (16.5)
Axial maximum-to-average power ratio	1.21
Maximum radial fuel power gradient, %/mm (%/in.)	0.4 (10)
Minimum radial power gradient	0
Total simulated fuel rod length, m (in.)	2.28 (90)
Simulated core length, m (in.)	0.99 (39.2)
Axial blanket length (each end), m (in.)	0.46 (18)
Simulated radial blanket rod length, m (in.)	2.28 (90)
Maximum power per fuel rod, kW	45
Nominal power per fuel rod, kW	36
Power per blanket rod, kW	11

3.1.3 Summary of Progress

3.1.3.1 Conceptual Design

The conceptual design of the CFTL, which was completed early in FY 1975, and is indicated in the flow diagram, Fig. 3.4, was summarized in an ORNL internal memorandum, "Conceptual Design Report for the Core Flow Test Facility." The Construction Project Data Sheet, originally 76-ORNL-1 but now 77-HNL-16, which presents the cost estimate, was completed also early in FY 1975. The dynamic simulation effort and layout work to support the conceptual design are reported in subsequent sections of this chapter.

3.1.3.2 Documentation

The Management Plan for the CFTL was submitted to the AEC in October 1974. The AEC concurred with our codes and standards proposal for the construction of the pressure retaining boundaries of the CFTL, which also was submitted to the AEC early in FY 1975. Energy Research and Development Administration (successor to AEC) has indicated agreement with our quality assurance plans, which were submitted in February 1975.

Section 1, Functions and Design Requirements, and Section 2, Design Description, of the CFTL System Design Description were submitted to ERDA and distributed as required.

Cost estimates prepared to construct the CFTL to capacities of either 61 or 92 FPSs, with expense or capital funding and for various construction starting dates, were furnished to RRD.

3.1.3.3 Development

The conceptual design indicated that development effort was needed for the helium circulators, test bundle simulators of the GCFR fuel and blanket pins, electrical power supply and control for the test bundle heaters, and the elimination or satisfactory reduction of electromagnetic noise on the temperature signals from the heater thermocouples. Reports on each of these efforts are presented in subsequent sections of this chapter.

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3.2 DYNAMIC SIMULATIONS FOR INITIAL DESIGN STUDIES OF THE CORE FLOW TEST LOOP — S. J. Ball and R. A. Hedrick

Computer simulations were used to predict the dynamic behavior of several proposed flow diagrams for the helium circulation system in the GCFR Core Flow Test Loop (CFTL) to assist in the selection of adequate loop configuration and control system. The primary design objective was to provide the capability of imposing rather severe, but carefully controlled, flow, temperature, power, and pressure transients on the test section while at the same time minimizing the deleterious effects of the transients on system pressure boundaries. The simulations indicated that satisfactory loop performance should be achieved with a configuration using two heat exchangers and three control valves; see Fig. 3.4. One heat exchanger will remove test bundle heat and the other, installed in a bypass line around the helium circulators, will remove circulator heat during certain modes of operation. The three valves will need to be provided with programmer-controlled fast moving stem actuators to achieve the required flow control of coolant helium during fast transient operation.

The transients prescribed for the CFTL present not only control problems but also the need to minimize the deleterious effects of the transients on loop pressure boundaries and components. Computerized simulations were made of several flow diagrams to help answer loop design and control option questions, such as the use of bypass lines, the locations of heat exchangers, flow control valves, and other components, and the coordination of stem position with time for the flow control valves. The results of the simulations were evaluated against two sets of criteria. On one hand, the operational requirements for the test section transients had to be satisfied. On the other hand, both the temperature transients experienced by the heat exchangers and circulators and the head and flow transients experienced by the circulators during power and flow modes of transient operations and system depressurizations needed to be limited to "acceptable values."

Analyses of loop thermal and flow transients used the IBM simulation language Continuous System Modeling Program (CSMP), which permitted an efficient programming effort and modest running times and costs.

3.2.1 Description of Analyses

3.2.1.1 Flow Control Valve Studies

Since the required flow transients for the test section are quite fast (up to 90% change in 1 sec), the use of either circulator speed control or flow feedback control of the loop valves was deemed impractical. Hence, a program was devised to calculate the stem position vs time for control valves FCV-1 and FCV-3 (shown in Fig. 3.4) required to give a specified test section flow transient and also to minimize changes in circulator head and flow.

The results of a calculation for a linear flow transient from 100 to 5% are shown in Fig. 3.5(a). Valve stem positions for FCV-1 and FCV-3, which control test bundle and bypass flows, are shown for both linear and equal-percentage valve trim. Equal-percentage trim is that for which equal increments of stem motion will produce equal-percentage changes in flow, assuming constant pressure drop across the valve. It can be seen from Fig. 3.5(a) that the stem position programs for both valves FCV-1 and FCV-3 would be more easily generated if equal-percentage trim were used.

Figure 3.5(b) shows similar results of a computation for a typical GCFR flow transient. The same conclusions regarding valve trim apply here also. In both cases, the control of circulator pressure change remained within 0.7 kPa (0.1 psi) of a 0.51 MPa (75 psi) operating point, and the circulator flow rate remained practically constant. Constant circulator speed was assumed.

3.2.1.2 Depressurization-Flow Studies

The simulation program was extended to include depressurization transients as well as constant-inventory flow transients. The object of the CFTL depressurization tests is to expose the test bundle to pressure and flow transients that simulate the GCFR design basis accident; the conditions seen by the rest of the loop do not necessarily need to mock up GCFR conditions. Hence, GCFR hot leg and cold leg ruptures could both be simulated on the CFTL by bleeding from a single point (near the test section inlet) and controlling the test section flow appropriately

Fig. 3.5. Flow Rate and Valve Stem Positions Versus Time for Flow Transients in CPTL. (a) Linear transient from 100% to 5%. (b) Typical GCPR transient.

with valve FCV-1. An important consideration is the effect of the depressurization on circulators (i.e., whether or not they can be kept within a safe operating range). Effects of loop temperature changes on the flow calculations are also neglected here, although they will be included in later more detailed analyses.

The results of a calculation in which the loop was depressurized from operating pressure to 0.2 MPa (2 atm) in about 1.5 min are shown in Fig. 3.6. In this run, FCV-1 was programmed to try to duplicate the test section flow transient indicated for a 0.032 m^2 (50 in.²) rupture in a GCFR, and FCV-3 was used to attempt to maintain a constant circulator head. The actual flow at the start of the transient falls short of the desired flow because the maximum opening of FCV-1 is limited. The circulator head variation was not serious (i.e., it went less than halfway between the operating point and the nominal thrust reversal limit). The simulation also assumed that the circulator speed controller

Fig. 3.6. Flow Rate and Valve Stem Position Versus Time During CPTL Depressurization from 9.0 MPa (1300 psia) to about 0.2 MPa (30 psia) in about 1.5 min.

was able to maintain a constant speed in spite of the rapid loss of load. This assumption should be checked when more data become available.

It may be concluded that reasonably good reproduction of the desired depressurization transients can be attained. If more accuracy is required at the start of the test, a larger flow coefficient for FCV-1 would be required. Rangeability requirements are no more stringent than for atmospheric pressure tests.

3.2.1.3 Test Section Simulation

The reference design for the test section was taken to be a 91-FPS fuel bundle with a typical simulator as shown in Fig. 3.7. These FPSs have relatively short thermal time constants (~ 1 sec at full flow), so fine-structure modeling of them was not crucial to calculating the transient behavior of the rest of the loop, which has much longer response times. However, a five-radial-node simulation was made both for comparison with a gross (single-node) model and for studying the effects on cladding temperatures of several proposed power control schemes during power transients. Three axial regions were used to approximate the temperature distributions along the flow path. At the time of the study, insufficient data on either surface roughening designs or local heat transfer coefficient (h) values were available, so a uniform, full-flow h of $8.5 \text{ kW m}^{-2} \text{ K}^{-1}$ ($1500 \text{ Btu hr}^{-1} \text{ ft}^{-2} \text{ }^{\circ}\text{F}^{-1}$) was assumed. It was assumed to be independent of temperature, but was varied as the 0.8 power of mass flow rate.

Figure 3.8 shows the calculated cladding temperature response to a power transient at full flow rate of test bundle coolant helium. Cladding cooldown rates of 178°C (320°F) in 1 sec and 411°C (740°F) in 4 sec are seen. The transients seen by the main loop heat exchanger would also be severe. This may indicate the need for a "flow scram" in case of a loss of heater power to avoid deleterious thermal quench of the bundle.

Control of the FPS bundle power was, for the initial simulations, assumed to follow the prescribed reactor power vs time values provided by GA. However, this assumption does not satisfy the test objectives

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NOTES:

1 ALL DIMENSIONS ARE FOR CO
2 BEATER FABRICATING S

Fig. 3.7. Heater for CFTL Prototypical GCFR Fuel Pin Simulator.

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1961-11-11	52:00	5200	52	52	52	52
1961-11-11	53:00	5300	53	53	53	53
1961-11-11	54:00	5400	54	54	54	54
1961-11-11	55:00	5500	55	55	55	55
1961-11-11	56:00	5600	56	56	56	56
1961-11-11	57:00	5700	57	57	57	57
1961-11-11	58:00	5800	58	58	58	58
1961-11-11	59:00	5900	59	59	59	59
1961-11-11	60:00	6000	60	60	60	60
1961-11-11	61:00	6100	61	61	61	61
1961-11-11	62:00	6200	62	62	62	62

NOTES:

- 1 ALL DIMENSIONS ARE FOR COMPLETED HEATER
2 HEATER FABRICATION SPEC UCLMFC

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Fig. 3.8. Calculated CFTL Heater Cladding Temperature Versus Time after Power Shutoff at Full Helium Flow Rate through Test Bundle.

because the stored energy in an FPS at nominal full-power conditions is only about one-tenth that in the (UO_2) fuel rod. Hence, a special computer program was written to predict the FPS power transients required to give the heat flux or cladding temperature vs time to simulate the behavior of a fuel rod. In effect, the additional energy input to an FPS in the modified power transient compensates for its lower stored energy. This calculation requires an assumption of the transient values of h for the entire bundle. It is expected that these can be derived by experiments. Various configurations were modeled, including partly roughened surfaces and nonuniform power distributions. In all cases, the compensated power control appeared to be feasible.

3.2.1.4 Total Loop Simulation

The total loop simulations combined the test section with simulations of the two air-cooled heat exchangers, the circulators, and the associated piping. Mass flow transients were assumed to be controlled as specified, as opposed to having flows calculated from valve positions, temperature-dependent flow resistances, etc. These features will be combined in later simulations.

The main loop heat exchanger model used four axial sections for accuracy at low-flow condition, while the bypass heat exchanger model used only one section. Selected simulation runs in which test section power and coolant helium flow rate are reduced simultaneously at approximately GCFK scram conditions were made primarily to show the temperature transients imposed at the circulator inlet and the inlet to the main heat exchanger (HX-1). We assumed that the cooling air flow rate to HX-1 is decreased in accordance with the test section transient, while the air flow rate to the bypass heat exchanger (HX-2) is increased simultaneously. Although the calculations show the helium temperature from HX-1 exit decreases quite rapidly [approximately 22°C (40°F) in 1 sec], we found that after attenuation by the pipe thermal inertia and dilution with the bypass helium flow (which rapidly reaches approximately 9 times the value of the main loop flow) the helium temperature at the circulator inlet decreases only about 5.6°C (10°F) in 20 sec.

The results of an example calculation of a simulated depressurization accident are shown in Fig. 3.9(a) and indicate a modest initial increase in cladding temperature 5.6°C ($\sim 10^{\circ}\text{F}$). For the actual case, the increase would probably be greater because of the greater stored energy in the fuel. The eventual maximum cladding temperature exceeds 843°C (1550°F) because of a long-term mismatch of flow and power. As was true of most transients run, the circulator inlet temperature was well-behaved, as may be seen in Fig. 3.9(b).

3.2.1.5 Loop Design Trade-Off Considerations

One of the primary reasons for the initial CFTL simulation was to evaluate various alternative designs. The object was to minimize the cost and complexity of the loop while still maintaining a capability for running the prescribed transient experiments. A series of simulations led to the elimination of an auxiliary heat exchanger paralleling the main loop exchanger, a heater-cooler unit upstream of the test section, and a second depressurization valve at the test section outlet. We showed that very little was lost in loop performance capabilities by eliminating these components. Further attempts to simplify the loop — e.g., to eliminate the circulator bypass heat exchanger — were not practical.

Fig. 3.9. Temperatures Versus Time in CFTL Simulated GCPR Depressurization Accident. (a) Simulator cladding temperature. (b) Helium temperature at circulator inlet.

3.2.2 Conclusions and Recommendations

Simulations of the transient behavior of the conceptual design of the CFTL indicate that the proposed loop arrangement and control schemes will make it possible to achieve the desired test section transients without imposing deleterious effects on the rest of the loop. While it is expected that a "satisfactory" conclusion would be also reached even with minor design changes and refinements in the simulation models, a more detailed simulation is planned after the loop design becomes more firm.

3.3 CONCEPTUAL DESIGN — C. W. Collins and J. A. Seneker

3.3.1 Loop Layout

A conceptual loop layout was prepared to meet the functional and design requirements for the CFTL. Figure 3.4 presents the schematic flow diagram of the CFTL helium system, showing the principal components and interconnecting piping of the helium flow paths and modes of operation for the CFTL. The principal path for coolant helium flow during normal operation is from the circulator, which is multiple single-stage equipment, through the test bundle in the test vessel, into heat exchanger 1 (HX-1), and back to the circulator inlet to repeat the cycle. During rapid transients, the circulator flow cannot be decreased fast enough by changing its speed, so the helium flow through the test section is changed by programmed flow control valves, which cause the helium flow to bypass the test vessel and HX-1 through the circulator bypass line, which includes heat exchanger 2 (HX-2). An attemperation flow of helium will enter the test vessel separately from the test bundle coolant helium to maintain the vessel at a constant temperature below 427°C (800°F) and will be mixed with the bundle flow at the test bundle exit. The purification helium will flow in a separate bypass line from the circulator outlet through the purification system and then will reenter the helium system at the circulator inlet. Valve FADV-1 permits rapid depressurization of the CFTL.

During both steady-state operation and the various simulated GCFR transients the helium coolant flow through the test bundle, which will

be measured with FM-1, will transport heat generated in the bundle to HX-1, an air-cooled heat sink designed to dissipate the maximum heat generation anticipated for the CFTL. The degree of mismatch that exists between GCFR reactor power and coolant helium flow rates during various transients described in the program plan will be simulated as closely as practical with test bundle power and helium flow rate.

As the test bundle flow is reduced to simulate a GCFR reactor trip, the circulator flow rate, which will be measured with FM-3, will be maintained nearly constant by increasing the flow rate in the circulator bypass line. HX-2 in this line will be used to remove circulator power during this time. Dynamic analysis indicates that the helium temperature at circulator inlet will change less than 27.8°C (50°F) during the simulated trip. Thus, the circulator operates at nearly constant conditions during a simulated trip without experiencing the rapid changes in temperature and flow rate seen by the test bundle.

During steady-state operation, a small attemperation flow in an annulus surrounding the test bundle will maintain the temperature of the test vessel pressure boundary nearly constant. The attemperation flow path will also be used in the approach-to-cladding-melting operation. This operation starts from steady-state design conditions, and at constant test bundle power the test bundle flow rate will be reduced and the attemperation flow rate will be increased in the same increments to maintain constant the total helium flow rate throughout the test vessel. This will protect the test vessel pressure boundary from the very high temperatures in the test bundle and also maintain the helium temperature at test vessel exit nearly constant by mixing the attemperation flow with the test bundle coolant helium. Thus the temperature and flow changes imposed on the test bundle during approach to cladding melting will be substantially reduced or essentially eliminated at HX-1 and at the circulator.

The impurities that are required by the program plan for the coolant helium will be introduced in a controlled manner into the helium system through the impurity fill station. The impurities may react for a time with the helium system components to make other chemical forms, which will be removed in the helium purification system from a small side stream

of helium to attain and maintain the prescribed impurity conditions. A mode of operation will be sought to maintain the impurity levels in the helium system while minimizing both the rate of impurity injection into the helium system and the flow of cleanup helium through the purification system.

The depressurization valve, FADV-1, will permit rapid reduction in the CFTL helium pressure from maximum operating pressure of 10.13 MPa (100 atm) to as low as 0.2 MPa (2 atm absolute) in as short a time as 60 sec.

A pipe size of 0.15 m (6 in.) was chosen for the helium system interconnecting piping over a 0.1-m (4-in.) size to reduce the heat requirement for the circulator; the velocity heads were about one-fourth as much with the larger pipe.

Coolant helium flow rate through the test bundle is regulated primarily with a valve having a fast-acting hydraulic operator that is controlled either manually by the operator or automatically with a programmer. In general, the circulator will be operated at constant speed to obtain the maximum coolant flow requirements for the bundle size being tested. The heat generation rate in the test bundle is regulated with an electronic controller that is either manually controlled by the operator or automatically controlled with a programmer. The manual controls permit the operator to obtain the various steady-state operating conditions of bundle coolant flow rate and power required for the CFTL. Simultaneous programmed regulation of the independent controls for coolant helium flow rate and test bundle power will be provided to permit simulation in the CFTL of all the anticipated GCFR flow and power transients. The programmed regulation will also control the cooling air flow rate for HX-1 and HX-2 during transient operation to minimize the change in helium temperature at the circulator inlet.

The test vessel inlet will be designed to assist attaining fully developed flow in the test bundle flow channels upstream of the start of the heated zone, which simulates the fueled section in a GCFR fuel pin. A mixer will be designed for the test vessel at the outlet of the

test bundle to mix the cooling attenuation flow from the annulus with the high-temperature helium from the test bundle. Also a full-flow filter will be installed in the piping immediately downstream of the HX-1 outlet to protect the downstream components from debris that would be produced if the bundle heater cladding actually melted during approach-to-cladding-melting operation.

The helium system components and interconnecting piping will be made of austenitic stainless steel for a design pressure of 11.72 MPa gage (1700 psig), which will provide margin for the operation of pressure relief equipment above the maximum working pressure of 10.34 MPa (1500 psia). The system components are sized to accommodate 32 kg/sec (7 lb/sec) of helium at 8.96 MPa (1300 psia) and 343°C (650°F) except the purification system, which is sized to accommodate helium flow rates up to a maximum of approximately 1 std liter/sec (2 cfm) at 10.34 MPa (1500 psia).

The heat sinks will be sized to reject 4700 kW for HX-1 and 600 kW for HX-2. A 4.1-MW electrical power and control system will be provided to supply the test bundle.

3.3.2 Loop Characteristics

Because the CFTL will be constructed to simulate power and coolant helium transients that are being anticipated in the design of the GCFR Demonstration Plant, thought was given in the conceptual design to enhance the reliable operation of various components of the helium system by protecting them as practicable from the deleterious effects of these transients.

Although both test bundle power and coolant helium flow rate are reduced simultaneously and programmatically in simulated GCFR transients, there is sufficient mismatching to change coolant helium temperature distribution in the test bundle. However, the controls of the system will be designed to maintain nearly constant the helium temperature at the inlet to the test bundle and both helium temperature and flow rate at the inlet to the circulator. The circulator bypass valve, FCV-3, will be opened to increase bypass flow rate as closely as practical to

the increment the test bundle flow rate is being decreased to maintain the helium flow rate at circulator inlet nearly constant. The flow rates of cooling air to HX-1 and HX-2 will be regulated to maintain the temperature of coolant helium at the test bundle inlet and circulator inlet as nearly constant as practical.

In an approach to cladding melting the bundle power is maintained constant while the coolant helium flow rate is reduced incrementally. The attenuation flow rate through the annulus that surrounds the test bundle will be increased incrementally so as to maintain constant the helium coolant flow rate through the test vessel, HX-1, and the circulator. Thus both the heat load on HX-1 and the circulator load will remain constant.

Results of preliminary dynamic analysis of simulating the GCFR depressurization incident indicate that there should be no significant changes in coolant helium temperature at the circulator inlet.

When the CFTL is scrammed, which will occur primarily upon loss of coolant helium flow to the test bundle or loss of helium system pressure, the signal will turn off the test bundle power automatically and immediately protect the simulators. The circulators and the cooling air blowers to HX-1 and HX-2 will be turned off, and the air flow control valves at the inlets to the blowers will be closed as appropriate to reduce the consequential effects of the thermal quench on the helium system components.

3.3.3 Loop Arrangement

Figures 3.10, 3.11, and 3.12 show the plan and elevation views of the CFTL conceptual design for the arrangement of the components and interconnecting piping in the helium system. The locations of the transformers and other switch gear for the loop electrical system, the control room and various controls, a crane for handling test bundles, and ducting and blowers for the heat sinks are shown also. The outline of the enclosure to protect personnel from contact with the high-temperature components of the loop may be seen also.

Fig. 3.10. Plan View, Proposed Layout GCFR Core Flow Test Loop (CFTL).

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Fig. 3.11. Elevation, Proposed Layout GCFR Core Flow Test Loop (CFTL),

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ation, Proposed Layout GCPR Core Flow Test Loop (CFTL),

Fig. 3.12. Plan View, Proposed Layout, Electrical System GCFR Core Flow Test Loop

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3.3.4 Loop Components and Systems

3.3.4.1 Heat Sinks

Each of the two heat sinks in the CFTL helium system will consist of a helium-to-air heat exchanger with the requisite blower, ducting, and controls for the cooling air. The larger heat sink, HX-1, located downstream of the test vessel exit, will reject the test bundle heat and helium circulator power to the cooling air. The smaller heat sink, HX-2, which will be located in the circulator bypass line, will reject circulator power during the flow transients when there will be a small flow of coolant helium through the test bundle and a large flow through the circulator bypass line. During normal operation there will be a small flow of helium through HX-2 to maintain the bypass line near system operating conditions.

The pressure-retaining boundary of the heat exchangers will be classified, designed, and constructed to the requirements for Class 2 Piping Components in Section III of the *ASME Boiler and Pressure Vessel Code*. The design pressure for both heat exchangers will be 11.72 MPa (1700 psia). The design temperature for HX-1 will range from 593°C (1100°F) at the inlet to 399°C (750°F) at the outlet. The design temperature for HX-2 will be 399°C (750°F).

The HX-1 will be designed to reject a maximum of 4.7 MW heat to a forced draft of ambient air while cooling a helium flow of 2.76 to 2.95 kg/sec (6.1–6.5 lb/sec from 538 to 316°C (1100–600°F)). The HX-2 will be designed to reject 0.6 MW heat to a forced draft of ambient cooling air while cooling a helium flow of 2.76 to 2.95 kg/sec (6.1–6.5 lb/sec) from circulator outlet to circulator inlet temperature.

For both exchangers ambient cooling air from outside the building will be moved by blower and ducted to the heat exchanger and then returned outside the building. The flow of air will be controllable with variable speed blowers and movable louvers or vanes installed at the cooling air inlet to the heat exchanger.

3.3.4 2 Helium Purification System

The CFTL must have the capability of duplicating the anticipated ranges of GCFR helium impurity levels. Since the CFTL helium will be obtained from the U.S. Bureau of Mines to high purity specifications, it will be necessary to introduce impurities into the helium and then to establish the required level of impurity by the use of the purification system.

The pressure-retaining boundary of the helium purification system will be designed for 11.72 MPa (1700 psig) and 399°C (750°F) and constructed and stamped in accordance to the rules of Section VIII, Division I, of the *ASME Boiler and Pressure Vessel Code* (the Code). Preliminary inquiries of U.S. industry indicated that the cost for construction of the purification system to the rules for Class 2 section of the Code would be higher than for construction to the rules of Division I, Section VIII. Because the purification system is ancilliary to the helium system and can be isolated from it with stop valves, we elected to use the rules of Division I, Section VIII of the Code.

The performance requirements for the purification system will be a maximum of 0.94 liter/sec (2 scfm) of helium flow at inlet conditions of 10.34 MPa (1500 psia), maximum and 343°C (650°F) maximum with a pressure drop of 2.75 kPa (40 psi). The inlet and outlet of the purification system will be connected, respectively, to the outlet and inlet of the circulator to provide the pressure drop. This performance should permit the mass of system helium to be processed through the purification system as fast as once per hour during a continuous two-week period before the system requires regeneration.

Preliminary information indicates that the purification system will probably consist of regenerative heat exchangers, a "Deoxo" catalytic purifier to convert hydrogen and oxygen to water, a copper oxide purifier to convert even more of the hydrogen and oxygen to water and carbon dioxide, a molecular sieve bed to absorb the generated water and carbon dioxide, and a low-temperature absorbent bed to remove the nitrogen, hydrocarbons, and other gases not collected in the molecular sieves.

3.3.4.3 Helium Supply System

The CFTL helium supply system will actually consist of two separate, although interconnected, subsystems: (1) the charging subsystem, which will provide the large amount of helium required for filling the loop and pressurization from atmospheric to operating pressure, and (2) the makeup subsystem, which will provide the helium required to maintain the loop at operating pressure.

Analysis of the present concept of the CFTL indicates that the volume of helium in the loop is approximately 2.5 m^3 (90 ft^3). The mass of helium needed to reach 10.34 MPa (1500 psia) and 343°C (650°F) would be about 20 kg (45 lb). The charging system will be capable of supplying this helium from a high-pressure helium trailer through a manifold station and interconnecting 25-mm (1-in.) piping to the fill port on the CFTL. The system will have the capacity to make four fills of the CFTL. Each fill will be made from atmospheric pressure to 4.82 MPa (700 psia) at room temperature and will be accomplished in less than 1 hr. A sufficient mass of helium will be introduced into the CFTL in this manner to achieve the desired operating pressures at operating temperature. The system will make use of the helium trailer rather than numerous small helium cylinders because the cost of helium used is much less when supplied from a bulk source.

The makeup helium system will be located just below the CFTL on the first floor of the site building. Tentatively, two banks of ten bottles of helium each will be installed onto a helium manifold and connected to the CFTL with 25-mm (1-in.) piping. Each bank would have the capability of injecting 1.8 kg (4 lb) of helium into the helium system in as short a time as 5 min with the CFTL pressure as high as 9.31 MPa (1350 psi) from fully charged bottles. Alternatively, the makeup helium will be supplied from the trailer.

3.3.4.4 Test Vessel

The CFTL test vessel, Fig. 3.13, will contain the test bundle that simulates a segment of a GCFR core element. The vessel will be cylindrical and will have bolted and seal-welded flanges at the top and bottom. Test bundles will be inserted through the top flange.

Fig. 3.13. CFTL Test Vessel.

Fig. 3.13. CPTL Test Vessel.

At the present time conceptual layouts of the test vessel are being made. The present design of the cylindrical portion of the vessel is a 0.25-m (10-in.) sched 120 stainless steel pipe, which appears to be large enough to accommodate all bundles to be tested. The total length of the test vessel, top flange to bottom flange, will be 3.30 to 3.81 m (130-159 in.).

Several concepts of the outlet mixing chamber are still being evaluated. Two such concepts are: a baffled arrangement downstream of the test bundle exit, and a perforated can between the two flow passages, which would allow the attemperation flow to be injected at relatively high velocity into the test bundle outlet flow. The inlet plenum is presently a volume containing the top portion of the simulator from the top flange tubesheet down to the first spacer grid in the lower portion of the bundle, referred to as the test section because it is the test simulation of a portion of a GCFR core element. Studies are under way to determine if this design will allow the helium flow to become fully established as in an actual core element before it reaches the heated section of the bundle approximately 0.76 m (30 in.) below the first spacer grid.

3.3.4.5 Piping

The piping that interconnects the various helium system components as shown in Figs. 3.4, 3.10, and 3.11 will be made of austenitic stainless steel and will be designed, constructed, and stamped in accordance with the rules for Class 2 Piping Components in Section III of the *ASME Boiler and Pressure Vessel Code*. The design conditions for the piping will be 11.72 MPa (1700 psig) and 399°C (750°F) except for the interconnecting run between the test vessel and HX-1, for which the temperature will be 593°C (1100°F). An all-welded piping design will be favored for the CFTL.

The piping will be sized to minimize velocity head friction loss as much as practical, and the piping runs will be designed to provide the upstream and downstream lengths that good design practice require for flow measurement devices.

There are three side nozzles welded to the vessel: the inlet nozzle located near the top flange, which carries coolant helium to the test bundle to remove bundle heat, the attemperation nozzle, which carries coolant helium to a path parallel to the bundle path to cool the test vessel pressure boundaries, and the outlet nozzle located near the bottom flange, which carries all the coolant helium out of the test vessel. The vessel exterior will be insulated to reduce test bundle heat losses and reduce the radial temperature gradient in the pressure boundary walls.

The inside diameter of the test vessel will be large enough to accommodate fuel test bundles of 19, 37, 61, and 91 simulators, a full-size blanket assembly of 127 simulators, and control rod test bundle of 54 simulators. In addition to the rod bundles, space shall be allowed for a thermal barrier installed around the bundles as well as an annulus between the thermal barrier and the test vessel, which will be a flow path for the attemperation or bypass flow. The thermal barrier and attemperation flow will be designed to maintain the pressure boundary below 427°C (800°F). The vessel pressure retaining boundary will be designed for 11.72 MPa (1700 psia) and 427°C (800°F) and constructed and stamped in accordance with the rules for Class 2 components, Section III, of the *ASME Boiler and Pressure Vessel Code*. An inlet plenum will be designed at the top of vessel to assure obtaining fully developed flow before the coolant helium reaches the heated zone of the test bundle. An outlet plenum or mixing chamber will be designed at the bottom of the test vessel downstream of the bundle outlet to mix the bundle and attemperation helium flows before the coolant leaves the test vessel.

The interior of the test vessel at the outlet plenum will be insulated to protect the high-temperature helium flow.

The simulator will extend upward through the vessel inlet plenum and through the top blind flange, which will also serve as a tubesheet. Individual simulators will be joined to the pressure boundary by use of trepanned ridges on the top surface of the tubesheet to which the heater sheath will be electron-beam welded.

At the present time conceptual layouts of the test vessel are being made. The present design of the cylindrical portion of the vessel is a 0.25-m (10-in.) sched 120 stainless steel pipe, which appears to be large enough to accommodate all bundles to be tested. The total length of the test vessel, top flange to bottom flange, will be 3.30 to 3.81 m (130-159 in.).

Several concepts of the outlet mixing chamber are still being evaluated. Two such concepts are: a baffled arrangement downstream of the test bundle exit, and a perforated can between the two flow passages, which would allow the attemperation flow to be injected at relatively high velocity into the test bundle outlet flow. The inlet plenum is presently a volume containing the top portion of the simulator from the top flange tubesheet down to the first spacer grid in the lower portion of the bundle, referred to as the test section because it is the test simulation of a portion of a GCFR core element. Studies are under way to determine if this design will allow the helium flow to become fully established as in an actual core element before it reaches the heated section of the bundle approximately 0.76 m (30 in.) below the first spacer grid.

3.3.4.5 Piping

The piping that interconnects the various helium system components as shown in Figs. 3.4, 3.10, and 3.11 will be made of austenitic stainless steel and will be designed, constructed, and stamped in accordance with the rules for Class 2 Piping Components in Section III of the *ASME Boiler and Pressure Vessel Code*. The design conditions for the piping will be 11.72 MPa (1700 psig) and 399°C (750°F) except for the interconnecting run between the test vessel and HX-1, for which the temperature will be 593°C (1100°F). An all-welded piping design will be favored for the CFTL.

The piping will be sized to minimize velocity head friction loss as much as practical, and the piping runs will be designed to provide the upstream and downstream lengths that good design practice require for flow measurement devices.

3.3.5 Loop Pressure Drop Analysis

Pressure drop losses around the helium system were calculated from the CFTL flow parameters of 30 g/sec (240 lb/hr) per simulator through the test section, 3.17 kg/sec (7 lb/sec) total maximum flow through the circulators, circulator outlet helium conditions of 343°C (650°F) and 8.96 MPa (1300 psia). Loop components that were analyzed on the basis of helium conditions existing at that location included the test section (consisting of that portion of the test bundle from the top spacer grid to the bottom spacer grid), test vessel inlet plenum, outlet or mixing plenum, flow control valve, flowmeters, large heat exchanger, full-flow filter, and system piping.

The test section losses were specified by GA to be a maximum of 278 kPa (40.4 psi) for all flow conditions of the CFTL operating envelope. This number is derived from the premise that the reference GCFR design is able to allocate this amount of the circulator head to the reactor core pressure drop. Since the CFTL operating envelope includes a much higher flow rate [30 g/sec (240 lb/hr) per simulator] than the reference GCFR nominal flow [23 g/sec (186 lb/hr) per rod, the flow cross-sectional area in the CFTL bundle would have to be increased to accommodate the increased flow to maintain the same pressure drop across the rod bundle.

The coolant helium flow path into the CFTL bundle simulators differs from the parallel entry into a GCFR core element in that the helium enters perpendicular to the simulators and must flow radially into the simulator lattice and turn in order to take the proper parallel flow direction before reaching the top spacer grid. Therefore, it was necessary to calculate the inlet plenum pressure loss based on this difference. The plenum resistance is due basically to three sources: expansion from the inlet nozzle, 90° change in direction, and friction from radial flow through the rod assembly. Total losses were calculated to be 18.6 kPa (2.7 psi) for the CFTL conditions.

The test vessel outlet plenum is also different from the GCFR design because its purpose is to promote mixing both the test section and the attemperation coolant streams. This is necessary because of the high temperature of the test section coolant flow, especially

during the approach-to-cladding-melting experiment, undercooling, etc. The pressure drop across the plenum depends on how the streams are mixed: in general, the better the mixing the larger the pressure loss. Calculations were based on a concept that would provide the best mixing, therefore the greater ΔP , with the resulting pressure drop of 46.8 kPa (6.8 psi). Any other practical mixing concept should produce a ΔP less than that stated above but would be somewhat less efficient in mixing the helium flows. In addition to the mixing action, further protection of the test vessel from high-temperature helium is obtained by insulating the pressure-retaining boundary of the outlet plenum from the helium.

The flow control valve located in the primary helium flow upstream of the test section will add resistance to the flow of helium, depending on the trim installed in the valve. The maximum ΔP of this component has been estimated to be 62 kPa (9 psi) for the range of flow conditions to be experienced in the CFTL.

Presently two flowmeter stations are located in the primary or main helium flow path, one upstream and one downstream of the attemperation flow tee. For the pressure calculation each station was assumed to consist of one venturi flowmeter and one target flowmeter, whose flow resistances are 7 kPa (1 psi) and 3.5 kPa (0.5 psi), respectively. Therefore, the total flowmeter ΔP is, based on present design, 21 kPa (3 psi). Ultrasonic flowmeters are presently being evaluated, and if we find that such units could be used in the CFTL, then the 21 kPa (3 psi) resistance would be essentially eliminated.

The 4.7-MW heat exchanger is a helium-to-air heat exchanger and as such requires large heat transfer area to reject its head load. For scoping and preliminary design, a heat exchanger was designed to provide the required surface area, coolant velocity, etc. This theoretical design was then used to calculate a base pressure drop, which was increased to 69 kPa (10 psi) to cover the maximum drop expected with a realistic exchanger design.

Information on the full-flow filter located downstream of the 4.7-MW heat exchanger has primarily been from filter manufacturers.

Present design specifies 15 μm absolute as the filtration requirement. Preliminary data indicate the pressure drop across this unit would be a maximum of 34.5 kPa (5 psi). The filter's primary purpose is to protect the helium circulators from particles that might be generated in the test section, especially during cladding melting experiments. However, the circulators presently being considered may be capable of ingesting particles of larger size without damage to bearing surfaces. If such is the case, then it may be possible to change to a coarser filter having a reduced pressure drop and less flow resistance.

The final component is the system piping. Present design has all piping to be 0.15 m (6 in.), sched 80 except for the section connecting the test vessel outlet to the heat exchanger inlet, which is 0.15 m (6 in.) sched 120. The proposed layout of the loop would result in a total pipe length of approximately 30 m (100 ft). Total pressure drop for the piping was calculated to be 17.2 kPa (2.5 psi).

The sum of the pressure drops of all the CFTL components based on the assumptions noted above is 547 kPa (79.4 psi) for the 8.96 MPa (1300 psia), 343°C (650°F), 3.17 kg/sec (7 lb/sec) flow conditions. The head rise requirement for the circulators is then 547 kPa (79.4 psi) or 7976 m (26,170 ft) based on circulator outlet helium conditions of 8.96 MPa (1300 psia) and 343°C (650°F). A corresponding analysis of the system was made for the 10.34 MPa (1500 psia), 343°C (650°F) based on the same flow rate. The head requirement for these conditions is 589 kPa (85.5 psi) or 7443 m (24,420 ft).

3.4 HELIUM CIRCULATORS — H. C. Young

Helium circulator activity was started in May 1974 with the preparation of a draft specification for a centrifugal machine supported on gas-film-lubricated bearings. Upon the request of AEC-RRD, the specification was broadened to include all types of gas-circulating machinery, and the UCND Purchasing Division made a preliminary inquiry in July 1974 to 17 circulator manufacturers. Three manufacturers responded positively and all opted for a centrifugal machine supported on gas-lubricated bearings. A formal specification for a centrifugal circulator supported on

gas-film-lubricated bearings was prepared, and a request for proposal was sent to the three manufacturers. All three responded with detailed proposals and were visited by an evaluation team in mid-April 1975. A cost-plus-fixed-fee contract for design, production, and testing of a prototype circulator was awarded to Mechanical Technology, Inc., of Latham, New York, in June 1975. Upon completion of the prototype circulator, a fixed-price contract will be negotiated for the three production units for the CFTL. MTL's original schedule indicated delivery of the production circulators would be in October 1978; however, this delivery date could be reduced to approximately February 1978 if additional funding for long lead items for the production circulators were provided by ERDA in early fiscal 1976.

3.4.1 Preliminary Inquiry

The general steps in the circulator procurement and the completion dates are listed in Table 3.2.

Specification M-11590-RB-001-S-0 for helium circulators for the CFTL was prepared, reviewed, and approved by project personnel by June 27, 1974. This specification was based on a hermetically sealed centrifugal machine in which the rotary assembly is supported on gas-film-lubricated bearings (gas bearings).

On the basis of a preliminary review of the specification, the AEC-RRD requested changing the base to include centrifugal, axial, reciprocating, and diaphragm circulators in order to invite manufacturers of all types of circulators to participate in the bidding. Accordingly, preliminary specification, M-11590-RB-002-S-0, which was completed on July 24, 1974, permitted the helium flow requirements to be supplied with either a single-stage, a multistage, or multiple single stage centrifugal, axial flow, reciprocating, or diaphragm circulators. Requirements were specified for helium purity, limit on amplitude and frequency of flow pulsations, helium leak rate to the atmosphere, and operational reliability during rapid depressurization of the CFTL and during an inlet temperature transient of 28°C/sec (50°F/sec) for 1 sec.

**Table 3.2. Steps in Placing a CPFF-Type Contract for
Helium Circulators for Phases I and II**

Step	Completion Date
1. Prepare preliminary circulator specification	June 1974
2. Issue preliminary Request for Quotation to solicit interest	July 1974
3. Receive preliminary quotations	October 1974
4. Revise circulator specification based on response to preliminary Request for Quotation	November 1974
5. Develop procurement package	October to December 1974
a. Circulator specification	
b. Purchase requisition -- approved by Division and Laboratory Directors	
c. Proposed scope of work (defined phases of job)	
d. Information required in proposal -- to permit evaluating manufacturers' overall competence	
e. Subcontract document -- to be signed by subcontractor	
f. Other legal documents -- includes terms and conditions, patents, certification of nonsegregated facilities, etc.	
6. Appointment of evaluation team	October 1974
7. Obtain approval from AEC-ORO to negotiate contract -- request for findings and determination	January 1975
8. Request for funds	January 1975
9. Issue Request for Proposal	January 1975
10. Receive and review proposals	March 1975
11. Evaluation of all interested manufacturers	April 1975
12. Place contract for Phases I and II	June 1975
13. Complete design, manufacture, and test of prototype circulator	March ^a or September 1977
14. Negotiate fixed price contract for manufacture and test of production circulators (Phase III) and installation (Phase IV)	March ^a or September 1977
15. Complete manufacture and test of production circulators	February ^b or October 1978

^aEarly date contingent on authorizing circulator supplier to start fabrication of pressure vessel early in Phase I.

^bEarly date contingent on additional funding for long-lead items for the production circulators being provided by ERDA early in fiscal 1976.

Some circulators were expected to require auxiliary equipment such as pulsation dampeners, filters, and additional helium purification capacity to remove impurities introduced by the circulator. Low-efficiency circulators would require a larger and more costly power supply and control.

A preliminary request for quotation was mailed to potential suppliers on July 30, 1974, by the UCND-Purchasing Division to invite industry's interest in participating in the design, manufacture, and testing of helium circulators for the CFTL. Quotations were originally to be supplied by August 29, 1974; however, several potential suppliers requested additional time, and quotations were accepted through mid-October, 1974.

Of the 17 potential circulator suppliers, three expressed definite interest in providing the circulators and all three proposed centrifugal machines supported on gas bearings. Six well-known manufacturers of reciprocating compressors and two well-known manufacturers of diaphragm compressors declined to bid. Some of the firms that produce reciprocating or diaphragm-type machines indicated that our application would be better provided with a centrifugal machine. The diaphragm compressor manufacturers stated that the number of large diaphragm compressors required, plus the equipment needed to dampen the flow pulsations to the required level, would make their pricing noncompetitive with the centrifugal machine.

One of the three interested circulator manufacturers proposed four centrifugal circulators arranged in series. A second manufacturer proposed either four circulators arranged in series or series-parallel. In each case, the choice of four circulators was based on the availability of basic motor designs of the type and power required.

Representatives of the third circulator manufacturer visited the UCND Purchasing Division to discuss their proposed concepts with project personnel. Their recommended choice was less conventional, with the motor mounted outside the circulator pressure-retaining boundary and a magnetic coupling employed to drive the motor through a ceramic cylinder that is part of the pressure boundary of the circulator vessel.

This manufacturer's second choice has the drive motor inside the pressure-retaining boundary. It is similar to the other two manufacturers' concepts, except that two impellers are mounted on a single shaft.

Based on information received with the preliminary quotations (i.e., that four circulators were required rather than three as anticipated) and the announcement at the October CFTL Information Meeting that project funding was reduced, studies were conducted to find ways of reducing the costs of the circulators. A review of the pressure losses in the CFTL piping, components, and test section indicated that the original assumption of basing pressure drop on the 10.34 MPa (1500 psia) condition yielded conservative, large values. Calculations of pressure drops in the CFTL based on the 8.96 MPa (1300 psia) condition indicated that three rather than four circulators in series should provide the GCFR requirements and also provide some margin for testing advanced core designs having increased pressure drop requirements. Also, in the interest of reducing circulators costs, each of the three interested manufacturers was asked about the possibility of changing from a variable frequency to a fixed-frequency power supply for the circulator drive motors and an estimate of the cost saving that might be realized by changing from the requirements of Section III, Class 2, to those of Section VIII, Division 1, of the ASME Code as applied to the circulator pressure-retaining boundary. Because of very high starting currents, a fixed-frequency power supply was not recommended. The estimated saving in changing code requirements was about 5%.

The upshot of the preliminary inquiry was to return to the gas bearing circulator. The original gas bearing circulator specification was revised, reviewed, and approved by project personnel. The major revisions included: basing the circulator head requirement on the 8.96 MPa (1300 psia) GCFR condition; an updating of reference to the Code to agree with the latest issue of Section III of the *ASME Boiler and Pressure Vessel Code*; change in the cumulative helium leakage across the pressure retaining boundary from 10^{-8} to 10^{-7} std ml/sec with the

circulator evacuated; and a relaxation in the specified maximum operating speed from 110 to 100% of design speed. Some principal circulator requirements are listed in Table 3.3.

Table 3.3. Some Principal Circulator Requirements

Design Item	Required Value
Pressure, MPa (psi)	11.7 (1700)
Temperature, °C (°F)	370 (700)
Service life, years	10
Flow rate, kg/sec (lb/sec)	3.2 (7.0)
Head required, kJ/kg (ft-lb/lb)	
for 8.94 MPa (1300 psi) outlet pressure	78.2 (26,170)
for 10.34 MPa (1500 psi)	73.0 (24,420)
Outlet helium temperature, °C (°F)	343 ± 4 (650 ± 10)

The Purchasing Division proposed and project personnel agreed that a four-phase procurement activity was indicated. A cost-plus-fixed-fee contract was proposed for the design and development phases. A fixed-price contract would then be negotiated for Phase III production of the working circulators.

Approval by AEC-ORO was obtained for the procurement activity, and an evaluation team was appointed by the Purchasing Division to evaluate all the proposals produced by this activity and to select the most promising.

3.4.2 Request for Proposal

On January 30, 1975, request for proposal T09061-78 was sent to:

Mechanical Technology, Inc.	- Latham, New York
Norwald-Turbo, Inc.	- Latham, New York
Shaker Research (team)	
AirResearch Manufacturing Company	- Torrance, California
Worthington Sales Company	- Atlanta, Georgia

The bid closing date was set for March 4, 1975. Worthington declined to bid. Because of the difficulty in finding in U.S. industry a developed drive motor, AiResearch requested a five-week extension of the bid period to April 7, 1975. AiResearch was notified that its proposal would be evaluated on the merits of its technical concept and on the capability and commitment of their company to produce the circulators. AiResearch then requested, and was granted, a two-week extension to the bid period. The other prospective bidders were notified of the change.

AiResearch indicated that it did not possess an ASME Nuclear Code (N) stamp and requested advice on the procedure for design and fabrication of the pressure-retaining boundary. Because none of the three bidders possessed the (N) stamp, all three were advised of three options available to them: (1) The bidder could apply to ASME to obtain a certificate of authorization to perform the design; then he could subcontract fabrication to a manufacturer having an N stamp. This process would require considerable time. (2) The bidder could subcontract both design and manufacture to a company that has ASME Code (N) stamp. (3) Bidder could subcontract design to company holding certificate of authorization from ASME to design vessel; then Bidder could authorize that company to subcontract fabrication to a third company having a Sect. III, Class 2, N stamp.

3.4.3 Proposal Evaluation

Proposals were received from all three bidders on the due date, March 18, 1975. Copies of each proposal were sent to members of the evaluation team and certain Laboratory specialists for review comments. Table 3.4 presents an approximate schedule for the evaluation activities.

The proposal reviewers met on April 8, 1975, to develop a set of questions to ask the bidders, and these questions plus a proposed agenda for on-site evaluation meetings were sent to each of the bidders. Rating sheets to evaluate each bidder had been developed earlier.

The evaluation team visited MTI and Norwalk-Turbo for two days each, April 14 to 18, 1975, AiResearch on April 21 and 22, and AiResearch's pressure vessel subcontractor on April 23. During the evaluation visit,

Table 3.4. Approximate Schedule for Evaluation Activities^a

Action	Date
Transmit copies of proposals to reviewers	Friday, March 21, 1975
Reviewers provide written comments	Thursday, April 3, 1975
Reviewers meet	8:30 am, Tuesday, April 8, 1975
Purchasing to alert bidders of visit by Evaluation Team	No later than April 8, 1975, for April 14-18 visits
Consolidate review comments	April 9-11
Evaluation Team visit to bidders	April 14-18
Evaluation Team prepares report	April 21-25
Evaluation Team submits report and recommendation for bidder selection to General Purchasing Agent	April 28
General Purchasing Agent request permission from ERDA-ORO to place contract	~May 1, 1975 ^b
ORO grants permission	~May 6, 1975 ^b
Place contract with successful bidder	~May 9, 1975 ^b

^aEvaluation Team: H. C. Webb, Chairman, Purchasing Division; A. Hopson, Purchasing Division; D. W. Burton, K-25; A. G. Grindell, ORNL; J. R. McGuffey, ORNL; H. C. Young, ORNL.

^bThese activities were delayed because of legal questions relating to licensing fees for a motor designed for an earlier government project.

each bidder was informed that the specified head developed by the circulators was to be increased by approximately 15%. The change in head was determined to be feasible and within the capability of each bidder's proposed concept. The Evaluation Team rated each bidder as soon as practical after each meeting.

The Evaluation Team submitted its technical report and recommendation on April 30, 1975, to the UCND Purchasing Division recommending that Mechanical Technology, Inc. (MTI) be awarded a CPFF contract for the design (Phase I) and the development (Phase II) of the prototype of the CFTL helium circulators, with the further requirement that MTI produce

three working circulators (Phase III) for the CFTL in a follow-on fixed-price purchasing contract to be negotiated at the completion of the prototype phase. The prototypic circulator would then serve as a spare.

A formal evaluation report was prepared, signed by all members of the Evaluation Team, and submitted to the General Purchasing Agent, UCND Purchasing Division, on May 2, 1975.

3.4.4 Contractual Negotiations

A required on-site preaward audit of MTI was completed, and MTI was informed by telegram that it had been selected.

The circulator specification was revised to include the new head requirements. In addition, as a result of discussions with the three bidders, the requirement that the bidder provide analysis to assure that the circulator has the capability of sustaining a 28°C/sec (50°F/sec decrease in inlet helium temperature for 1 sec without damage to the circulator was added to the specification, and the requirement that this capability be proven experimentally was deleted from the specification.

The revised specification was forwarded to MTI, and the UCND Purchasing Division then began negotiating the actual contract with MTI, considering the revised specification, terms and conditions, patents and licensing of the drive motor design from General Electric Company, fee, and a request by ERDA to record test data on magnetic tape. After adjudicating the licensing agreement, the contract was signed for UCND on June 4, 1975, signed by MTI on June 6, 1975, and approved by ERDA-ORO on June 17, 1975.

The schedule, Table 3.4, was fairly well adhered to through the submission of the evaluation team report on April 30. However, a substantially longer time than estimated to negotiate a licensing agreement for the drive motors resulted in contract approval four weeks later than estimated.

3.4.5 Description of Proposed Circulator Concepts

All three bidders for the helium circulators, AirResearch, Mechanical Technology, Inc. (MTI), and Norwalk-Turbo, Inc., proposed centrifugal

machines supported on helium-lubricated self-acting journal and one-way thrust bearings. Each of the two vertically oriented AiResearch circulators uses a 149 kW (200 hp) electric motor and a design speed of 23,750 rpm. The MTI concept shown in Fig. 3.14 requires three circulators in series, each with ~180-hp (134 kW) drive motor. The design speed is 23,500 rpm and the shaft has a near horizontal orientation. The Norwalk-Turbo concept is very similar to the MTI unit and requires four circulators in series, each with 93.4 kW (125 hp) drive motor. Norwalk-Turbo chose a design speed of 18,000 rpm.

3.4.6 Schedule Improvement

The original schedules indicated that each bidder would require approximately 39 months after start of the contract to complete Phase IV; that is, approval of final installation of the production circulators in the CFTL.

We were requested by ERDA and General Atomic to determine the feasibility of improving the circulator schedule by 6 to 12 months over the 39 month schedule.

The successful bidder, Mechanical Technology, Inc., stated that the overall schedule can be reduced from 39 to 30 months provided we approve within three months after the start of Phase I design the purchase of critical long delivery items for the three working circulators that are to be produced at fixed price in Phase III of the contract, and provided ORNL will lend MTI a variable frequency power supply within 12 months of start of Phase I design. The power supply will be used by MTI for performance testing of the prototype Phase II circulator and the three Phase III working circulators. Then it will be returned to ORNL and installed at the CFTL site.

The long-lead items for Phase III are the pressure vessel material, fabrication of the pressure vessels, impeller forgings, and drive motors.

To shorten the overall schedule for Phases I, II, and III of the helium circulator procurement from 39 months to 30 months would require authorizing the expenditure early in Phase I of \$136,000 of Phase III funding by MTI plus \$37,000 for procurement of a variable-frequency power supply by ORNL.

machines supported on helium-lubricated self-acting journal and one-way thrust bearings. Each of the two vertically oriented AirResearch circulators uses a 149 kW (200 hp) electric motor and a design speed of 23,750 rpm. The MTI concept shown in Fig. 3.14 requires three circulators in series, each with ~180-hp (134 kW) drive motor. The design speed is 23,500 rpm and the shaft has a near horizontal orientation. The Norwalk-Turbo concept is very similar to the MTI unit and requires four circulators in series, each with 93.4 kW (125 hp) drive motor. Norwalk-Turbo chose a design speed of 18,000 rpm.

3.4.6 Schedule Improvement

The original schedules indicated that each bidder would require approximately 39 months after start of the contract to complete Phase IV; that is, approval of final installation of the production circulators in the CFTL.

We were requested by ERDA and General Atomic to determine the feasibility of improving the circulator schedule by 6 to 12 months over the 39 month schedule.

The successful bidder, Mechanical Technology, Inc., stated that the overall schedule can be reduced from 39 to 30 months provided we approve within three months after the start of Phase I design the purchase of critical long delivery items for the three working circulators that are to be produced at fixed price in Phase III of the contract, and provided ORNL will lend MTI a variable frequency power supply within 12 months of start of Phase I design. The power supply will be used by MTI for performance testing of the prototype Phase II circulator and the three Phase III working circulators. Then it will be returned to ORNL and installed at the CFTL site.

The long-lead items for Phase III are the pressure vessel material, fabrication of the pressure vessels, impeller forgings, and drive motors.

To shorten the overall schedule for Phases I, II, and III of the helium circulator procurement from 39 months to 30 months would require authorizing the expenditure early in Phase I of \$136,000 of Phase III funding by MTI plus \$37,000 for procurement of a variable-frequency power supply by ORNL.

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HELIUM CIRCULATOR FOR GCFR-CORE FLOW TEST LOOP (CFTL)

Fig. 3.14. Helium Circulator for GCFR Core Flow Test Loop, CFTL.

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**HELUM CIRCULATOR
FOR
GCFR-CORE FLOW TEST LOOP (CFTL)**

Helium Circulator for GCFR Core Flow Test Loop, CFTL.

The additional funding required early in FY 1976 would be:

3 circulator drive motors	\$ 24,000
3 pressure vessels (material and fabrication)	111,000
impeller forgings	1,000
1 variable-frequency power supply	37,000
	<hr/>
	\$173,000

Funds for the activities in Phases I and II are already available in the FY 1976 "A" budget.

Mechanical Technology, Inc.. will propose approximately 2.5 months after receipt of the order for Phases I and II the detailed requirements for the critical long lead items that permit the schedule improvement.

We could authorize MTI to order only the pressure vessel material for Phase III; however, MTI suggests that some cost is saved by ordering fabrication of all four pressure vessels simultaneously. They indicate there is essentially no risk that prototype tests would cause a change in the pressure vessel design.

A request for additional funding will be issued by ORNL immediately after ERDA-ORO approves the contract with MTI.

3.5 HEATER DEVELOPMENT FOR GCFR FUEL PIN SIMULATORS · D. L. Clark and R. W. McCulloch

Request for design of a GCFR fuel pin simulator (FPS) was received in January of 1974. This report covers the subsequent design, materials selection, FPS specification, quality assurance, and procurement activities needed to develop the fuel pin simulator. Two other projects directly related to the GCFR fuel pin simulators were active in this report period. These are: the design and procurement of a mockup GCFR fuel pin simulator, which will be used to evaluate thermocouple noise and accuracy to select the optimum thermocouple design, and the modification of the LMFBR FPS Test Facility to enable testing of the larger diameter GCFR FPS.

Because of the similarities of the FPS, the previous development at ORNL of the electrical heaters for the LMFBR fuel pin simulators provided a significant base for most of the efforts in this work. Wherever practical (and in essence this was so throughout this time period), all

developments of GCFR fuel pin simulator are coordinated with the continuing efforts in improving the LMFBR fuel pin simulators. To the extent feasible, this was also true for the ORNL effort in PWR fuel simulators for the Flow Down Heat Transfer and Multirod Burst Test programs.

3.5.1. Fuel Pin Simulator Design

The preliminary design of this FPS was based on the successful design being used for the LMFBR Category II FPS, as shown in Fig. 3.15. The larger diameter of the GCFR fuel pin provides sufficient cross-sectional area for the inclusion of a central electrical conductor in a coaxial configuration with the other electrical conductor (and the heating element). The selected design for the FPS prototype is shown in Fig. 3.7 (p. 88).

This design was discussed in detail in meetings with GA and ERDA-RRD personnel as well as with each of the potential heater vendors. Fabrication details were worked out, and appropriate design corrections were made. Some potentially difficult assembly problems remain, but each of the six potential vendors is aware of these areas and is working out his own solution.

The FPS is designed to accommodate a multiple number of internal thermocouples for measuring the temperature of the internal surface of the sheath. The measuring junction may be grounded, insulated, or intrinsic; the type will be determined on the basis of tests that will be conducted on mockup FPSs are covered in Sect. 3.5.6. The thermocouple will be ISA type K sheathed, 0.51 or 0.38 mm OD (0.020 or 0.015 in.). These thermocouples should be capable of accurate measurements up to approximately 816°C (1500°F) and reduced accuracy at higher temperatures.

The FPS lead design is based upon one side of the power supply being at ground potential (thru suitable grounding resistors) and with that side of the power supply connected to the outer (tubular) lead-in conductor. Thus the insulation thickness for this tubular lead-in conductor can be reduced to a very thin but controlled layer.

The FPS lead-in conductor configuration is based on a relatively short external length [less than 25 mm (1 in.)] of the outer tubular lead-in conductor before its connection to an adequately cooled bus.

Fig. 3.15. LMFBR Category II Bundle 6 Fuel Pin Simulator.

Further analysis will be made as required to ensure against overheating of either lead-in conductor external to the heater sheath.

The heater design is based on 45 kW at 250 V and 180 A (approximately), but variations can be accommodated by changing the heating element ribbon design. The heating element ribbon thickness was selected to be 0.25 mm (0.010 in.) to provide suitable electrical clearances for the BN insulation without being too fragile. Previous experience with heating elements of this approximate size was a further contributing factor.

3.5.2 Materials Selection

This development included investigation and selection of materials for the heating element, the two coaxial heater lead-in conductors, the intermediate materials (between the heater lead-in conductors and the heating element), the heater sheath material, the grade and type of boron nitride (BN) insulation, studies to determine the optimum BN density (and uniformity), and the continuing development of cold-pressed tubular and rod preforms of BN.

3.5.2.1 Heating Element Material

On the basis of previous research for the LMFBR fuel pin simulators, the investigations into heating element material were limited primarily to three candidate materials:

1. Nichrome V, a heating element material consisting of 80% Ni and 20% Cr, melts at approximately 1400°C (2550°F) and is essentially the industry standard for heating elements. This alloy has excellent fabrication qualities relative to forming, machining, ductility, weldability, brazeability, etc. It has a very low temperature coefficient of resistivity [less than 6% total from ambient to 1370°C (2400°F)] and it is stable toward pure BN up to its melting point. Known reactions with impurities in previous grades of BN (and possibly with some present grades) occur primarily above 1260°C (2300°F).

2. Kanthal A-1, heating element material consisting of iron, chromium, and aluminum, melts at approximately 1510°C (2750°F) and is used primarily for higher temperature requirements. Also, its lower cost leads to use

in certain other applications. This material is much more difficult to work with as it is relatively brittle and, for all practical purposes, cannot be welded. Its temperature coefficient of resistivity is very low (slightly lower than Nichrome V). It was specifically designed for use in oxidizing atmosphere, where the aluminum would be converted to aluminum oxide on the surface and provide an effective barrier against further oxidation. There are no known problems with compatibility between BN and Kanthal A-1, but this has not been evaluated fully on a time-temperature basis. Other similar alloys like Hoskins 875 have also been considered.

3. The resistance alloy Pt-8% W (trade name Sigmund Cohn Alloy 479) melts at 1760°C (3200°F) and is normally not considered as a heating element material. In the annealed state, it has reasonable forming and machining characteristics. It is easily weldable to materials of interest and is very compatible with candidate BN powders. Other than high cost, the main objection to its use is its high temperature coefficient of resistivity compared with the other candidate materials (see Fig. 3.16). In the development of LMFBR fuel pin simulators, some research was performed to optimize the tungsten content and to add rhodium as a third constituent, both for the prime purpose of lowering the temperature coefficient of resistivity. The addition of more tungsten results in an alloy that is so hard that normal rolling and reduction techniques are ineffective. Limited funding has permitted only a minimal research effort on the tertiary alloy, and this effort was primarily restricted to the alloy Pt-8% W-26% Rh.

Since the Nichrome V melts at a temperature well above the calculated steady-state temperature for the heating element (see Fig. 3.17) and also above the calculated temperature for the heating element in the first transient tests, it has been selected as the initial heating element material. Also the weldability of Nichrome V is of extreme importance because of the minimal tolerances permitted by the heater design.

Subsequent test requirements, such as cladding relocation tests, will require the use of heating elements having higher melting temperatures, and these will be evaluated as required.

Fig. 3.16. Electrical Resistivity of Pt-8% W.

Fig. 3.17. Steady-State Temperature Profile from Center Line of Heater to Outer Surface of Stainless Steel Cladding at 39 kW/m (12 kW/ft) for GCPR Fuel Pin Simulator.

3.5.2.2 Fuel Pin Simulator Lead-In Conductors

The FPS lead-in conductors conduct the current to the heating element or to the intermediate material, in this case, without melting or otherwise deteriorating, while generating a minimal amount of heat. In the coaxial heater lead-in conductor arrangement, the latter is of extreme importance (see Sect. 3.5.1).

The ideal material would be silver, with its low resistivity and excellent fabrication qualities. However, copper is much less expensive, has a higher melting point 1083 vs 950°C (1980 vs 1760°F), and in the proper grade it can meet all the requirements without exception. External lead-in lengths will be minimized to increase the heat sink capability of the water-cooled connecting bus bars presently envisioned. This in turn will effectively limit the temperature of the two coaxial copper lead-in conductors for each heater.

3.5.2.3 Intermediate Materials

The intermediate materials must have melting points above that of the heating element material and should also have a reasonably low volume electrical resistivity. Molybdenum would be the optimum material from these two viewpoints, with tungsten a close second choice. However, welding (or other attachment technique) problems exist with molybdenum, as evidenced by previous usage (and attempted usage) in the LMFBR fuel pin simulators. Fabrication problems and the poor availability of suitable tubular forms (for the outer intermediate material) further contributed to the decision to not use either of these materials.

Nickel meets the above two stated requirements and is easily fabricated and welded or brazed. On the basis of experience in the LMFBR heater designs, it is compatible with the BN at temperatures of interest. It is available in a multitude of sizes and shapes and in the desired grades, such as Nickel 200.

3.5.2.4 Fuel Pin Simulator Sheath Material

The FPS sheath material was essentially dictated by GA program direction as type 316 stainless steel with a nominal wall thickness of

0.76 mm (0.030 in.) in the finished FPS. The diameter of the initial or starting tubes will be selected to provide 20% reduction during swaging. Heat transfer enhancement and any other surface preparation will be performed by GA on the completed FPS.

3.5.2.5 Electrical Insulation

Only boron nitride has been considered for the electrical insulation of these FPSs. The low values of thermal conductivity of magnesium oxide and aluminum oxide (as well as the variation of their thermal conductivity with density) preclude their use in high-heat-flux heaters that must operate at high temperatures under transient as well as steady-state conditions. Beryllium oxide was not considered because of its toxicity, hardness, and high cost.

Previous research with BN powder (and hot-pressed material) on the LMFBR and PWR fuel pin simulators has developed several candidate BN materials and has eliminated other possible BN materials and types. The required BN material must be very pure and cannot undergo phase changes below the maximum required operating temperatures. This eliminated UCC type HCT-48 (also known as type TS-945) and the only known Carborundum BN powder.

Newer BN powders such as UCC type HD-0092 and type IS-1206 have yielded satisfactory results when tested in sample LMFBR heater configurations for insulation resistance and compatibility with heating element material at temperatures up to approximately 1427°C (2600°F). However, these materials will not normally be available from UCC in the future, so alternate materials were considered.

Boron nitride of UCC type HCM, which is made similarly to UCC type HD-0092, will be available in quantity in the future. Evaluation of FPSs of the LMFBR design made with the HCM powder has yielded satisfactory results on both nondestructive and operational test bases. In laboratory tests, the powder has been cold-pressed into preforms having satisfactory strength.

This and other types of powders will be evaluated further early in FY 1976 by use of "conductivity cells," which will be received the

last of FY 1975. The "conductivity cells" will be used to measure thermal conductivity of the compacted BN powder as a function of density and of the type or grade of BN powder as well as to measure the electrical conductivity as a function of temperature to 1427°C (2600°F). This high-temperature testing will also provide compatibility data. These "conductivity cells" are being fabricated by two different manufacturers to compensate vendor-dependent characteristics.

The design of the GCFR fuel pin simulators was based in part on the use of cold-pressed BN preforms. Research and development in this area were initiated earlier in the LMFBR program and will be continued. Laboratory results have been very promising and are being discussed in detail with the potential commercial vendor. When suitable preforms are available, manufacturers using these preforms will fabricate additional "conductivity cells" to evaluate their use in heater configuration.

Upon completion of the analysis of the data obtained from the "conductivity cell" and the BN preforms, we can optimize the BN density and select the BN type. Effects due to any anisotropic characteristics achieved can be properly evaluated as well as the relative uniformity of any characteristic or property of the candidate materials.

As previously noted, this development will be a coordinated effort with the programs for development of other types of fuel pin simulators at ORNL.

3.5.3 Fuel Pin Simulator Specification

A specification was written to detail the procedures for material selection, fabrication, and nondestructive testing of the GCFR fuel pin simulators. It includes a series of quality assurance procedures detailing inspections performed or witnessed at the factory by our on-site inspector. This specification was reviewed in a meeting with GA in April 1975, and is being revised.

3.5.4 Quality Assurance

Applicable quality assurance covers design, materials procurement, FPS fabrication, and nondestructive testing of finished units. In

addition to the FPS specification, the following standards apply in total or in part to pertinent FPS materials:

RDT F 3-6 T Nondestructive Examination,
 RDT M 3-2 T Seamless Ferritic and Austenitic Alloy Steel Tubes,
 ASTM B 75 Seamless Copper Tube,
 ASTM B 152 Copper Sheet, Strip, Plate, and Rolled Bar,
 ASTM B 160 Nickel Rod and Bar,
 ASTM B 161 Nickel Seamless Pipe and Tube,
 ASTM B 344 Specification for Drawn or Rolled Nickel-Chromium and
 Nickel-Chromium-Iron Alloys for Electrical Heating
 Requirements,
 ANSI C 96.1 Temperature Measurement Thermocouples,
 QQ-N-209A Nickel Plating.

The materials are being ordered to these specifications and are inspected in accordance with them to assure conformity of the FPS to the design configuration, FPS-to-FPS uniformity, and reliability of the internal thermocouples.

Detailed specifications are not yet available for the BN powder so each incoming lot is analyzed to determine impurity levels of carbon, oxygen, and many other elements that could be detrimental to its properties. In addition archive samples are kept to aid in later evaluation if problems occur.

Fabrication will be closely monitored by an on-site inspector, who will witness or perform inspections in accordance with written QA inspection procedures after every major fabrication step. Problems found with BN density, material dimensions, insulation resistance, etc. will be corrected before the next processing step.

After the finished FPS has been received, radiographs, infrared scans, and thermocouple junction location searches will be made before acceptance. Radiographs show up such defects as coil eccentricity, pitch perturbations, voids in core insulation, and some defects in material interfaces. Infrared scans are used to determine variations in BN density, thermal conductivity, and cladding thickness as well as coil pitch perturbations. The thermocouple search test consists of using a heat

source and measuring thermocouple response to the heat. The maximum response locates the thermocouple junction. Junctions can be located to within 3 mm (1/8 in.) by this method.

Work is in progress to automate the infrared (IR) facility and to provide an automated x-ray radiography system. These modifications, when completed, significantly increase throughput capability as well as accuracy.

The radiography facility generates a small-diameter x-ray beam which is passed through an FPS moving at a fixed rate. The beam is detected after passing through the FPS, and an analog voltage is produced. The output can be used to very accurately determine ribbon spacing, width, turns per unit length, and eccentricity.

The automation of both the IR and radiography will consist first of obtaining equipment to produce a digital output of the signal and second of providing software to convert the raw data to suitable output information. These should both be completed in time to evaluate the first GCFR FPS.

3.5.5 Procurement Activities

Procurement and testing of final production fuel pin simulators are the last steps in a series of prior activities, which include: (1) procurement of long-lead-time and/or critical materials, (2) discussion of design with manufacturers, (3) procurement of prototypes from participating manufacturers, and (4) testing and evaluation of prototypes in one or more rounds to select a qualified vendor or vendors for production heaters.

Procurement of the Nichrome V heating element, the stainless steel sheath, and the BN insulation is complete. Grade HCM BN powder has been obtained for use in the prototypes. Preforms will be obtained for production heaters if they are manufacturable and their properties are better than those of the powder, as expected. The lead-in and return elements were considered noncritical and will be handled by the manufacturers.

Several discussions have been held with manufacturers to work out details of design and fabrication. Purchase requisitions for prototypic heaters were sent out to six manufacturers, and purchase orders have

been given to five of them: RAMA Corporation of San Jacinto, California; SEMCO of North Hollywood, California; Watlow Electric Manufacturing Company of St. Louis, Missouri; General Electric Company of Seattle, Washington; and Advanced Heat Concepts, Inc., of Addison, Illinois. Materials for the heater sheath, heating elements, and BN have been sent to these manufacturers, and fabrication is in progress. The first units of these prototypes are due in the first quarter of FY 1976, and evaluation will begin upon receipt.

3.5.6 Thermocouple Noise Evaluation

Mockup FPSs have been designed and procured for characterizing electromagnetic interference noise (emi) induced on different types of thermocouples inside the heater by different candidate power and control systems that will be evaluated for the CFTL test bundle application.

Three different types of thermocouple installations are provided inside each FPS. One is an intrinsic thermocouple consisting of two individually sheathed 0.38-mm-OD (0.015-in.) type K thermoelements, each of which is spot-welded to the inside surface of the heater sheath. The other two thermocouples are sheathed 0.51-mm-OD (0.020-in.) type K thermocouples, one with a grounded junction and the other with an insulated junction.

The FPS configuration is similar to that for the prototypic GCFR FPS, except that all current-carrying components (including the coiled heater element) are fabricated from copper, and magnesium oxide insulation is used in lieu of BN. The unit will be operated at rated current conditions to mock up the magnetic fields that would be present in the bundle heater. Electrical fields can be imposed by applying an appropriate potential between the heater sheath and one of the copper lead-in conductors.

Fabrication details of the mockup FPS were worked out with the RAMA Corporation, and a prototypic FPS was evaluated. Minor changes were made, and RAMA has fabricated ten such units. Problems arose in connection with the attachment of thermocouples to the inner sheath, but RAMA resolved the problems.

The mockup FPSs will be assembled in a seven-rod bundle. These will be operated in various test modes with differing numbers of FPSs and different power and control systems to analyze thermocouple noise. Appropriate filter circuits to eliminate the noise or reduce it satisfactorily will be designed and tested as required. Based upon these results, the best thermocouple design will be employed in the second round of prototypes and in the production FPS.

3.5.7 Test Section for Fuel Pin Simulator Test Loop

To evaluate the operational characteristics of the GCFR prototypic FPS without incurring large financial costs, the vertical test section in the existing LMFBR FPS Test Facility was modified to the increased diameter and length of the GCFR FPS compared with the FFM FPS. The design was completed in February 1975, the required materials were obtained, and the fabrication activities were completed in June 1975. The vertical test section will be installed in the loop in time for prototype tests, which are scheduled to begin in the second quarter of FY 1976.

3.6 ELECTRICAL POWER AND CONTROL SYSTEM FOR CFTL TEST BUNDLE — T. L. Hudson and J. R. Stansberry

The operating conditions of the test bundle require a source of electrical power that can be accurately controlled and quickly changed over a wide range of power levels to provide the power transients required of the CFTL. Of the various techniques studied and still being considered are those employing silicon-controlled rectifiers (SCRs). Because electromagnetic interference (emi) noise is seen on the FFM heater cladding thermocouples with the installed SCR systems, it was decided to make the CFTL power supply and control system the subject of development effort. The status of both the ac and dc power supply and control systems being considered and tested for evaluation is reported.

3.6.1 Test Bundle and Control Criteria

The criteria for the power supply and control for the fuel, control rod, and blanket test bundles for both flat (constant power across bundle)

and skewed (decreasing power level across bundle) power distributions and for both steady-state and transient operation have been developed and are shown in Tables 3.6 and 3.7. Example: A test bundle consisting of 91 heaters will be controlled in 11 zones (which corresponds to the number of rows of heaters from flat to flat across the hexagonal bundle configuration), while a bundle of 19 rods will be controlled nominally in five zones. The maximum bundle power demand, (ninety-one 45-kW heaters), will be 4100 kW, with each zone rated at 500 kW maximum.

Table 3.6. Criteria for the Power Supply of CFTL Test Bundles

Bundle	Maximum Power, kW		Number of Rows of Heaters	Number of Heaters	
	per Heater	Total		Total	per Row
Fuel	45	4100	11	91	6, 7, 8, 9, 10, 11, 10, 9, 8, 7, 6
Control Rod	45	4050	13	90	7, 8, 9, 6, 6, 6, 6, 6, 6, 9, 8, 7
Blanket	11	1400	13	127	7, 8, 9, 10, 11, 12, 13, 12, 11, 10, 9, 8, 7

Table 3.7. Criteria for the Control of CFTL Test Bundles

Mode of Operation	Criteria
<u>Steady-State Operation</u>	
Flat power distribution	Any power level between $100 \pm 1\%$ and $5 \pm 0.1\%$ of maximum power
Skewed power operation for fuel and control fuel rods	Skewed power slopes at 0.20, 0.26, 0.30, and 0.39% per radial millimeter (5, 6.5, 7.5, and 10%/in.) across the bundle Distance across flats 84.91 mm (3.343 in.) for fuel rods, 101.88 mm (4.011 in.) for control fuel rods, and 149.12 mm (5.871 in.) for blanket rods Individual runs at 100, 80, 60, 40, 25, 10, and 7% of maximum power
Skewed power operation for blanket rods	Skew power from 11 kW near face of fuel rods to 3.3 kW at the far face
<u>Transient Operation</u>	
Flat power distribution	Power changed from $100 \pm 1\%$ to $10 \pm 1\%$ of maximum power in 1-, 2-, 4-, and 16-sec time periods
Skewed power	Transient run at 100 and 60% maximum power for both 0.26 and 0.39%/mm (6.5 and 10%/in.) slopes

3.6.2 Power System

A new substation consisting of two 2500-kVA, 3-phase, 13.8-kV-to-480-V, self-cooled pad-mounted transformers will be located outside the building and will be connected to the existing 40-MVA transformer located within 53 m (175 ft) of the CFTL at the southeast corner of Building 9201-3. The two transformers will be equipped with eight raise and eight lower load changing taps to provide $\pm 7.5\%$ total change in the secondary voltage of the substation. A one-line diagram of the power system is shown in Fig. 3.18.

Fig. 3.18. One-Line Diagram of Power System for GCFR Core Flow Test Loop (CFTL).

3.6.3 Power Control Criteria

During steady-state operation the test bundle power control (the control) must provide for adjustment in power level from 5 to 100% of design power. Also, for skewed distributions power levels must be adjusted from 1.67 to 3.34% between adjacent FPS rows. During

transient operation, the control must change power from 100 to 10% of design level with 1-, 2-, 4-, and 16-second intervals for both flat and skewed distributions from predetermined set points.

3.6.4 Development and Testing

Cladding temperature is important to measure during a simulated GCFR reactor trip from 100 to 10% of design power in as short a time as 1 sec. Also, electromagnetic interference (emi) noise is seen on heater cladding thermocouple signals from test bundles in the LMFBR Fuel Failure Mockup (FFM) with the installed ac power supply, which uses analog-controlled silicon-controlled rectifiers (SCRs). Therefore, we decided to make the CFTL power supply and control system the subject of engineering studies and development effort. The following four systems were studied and discarded.

DC generators	Expensive and best for large load
Autotransformers	Voltage is adjusted by mechanical means and not suitable for fast power transients.
Induction regulators	Voltage is adjusted by mechanical means and not suitable for fast power transients.
Saturable reactors	Nonlinear device, best power control exercised near rated current, not suitable for fast power transients.

Both ac and dc power supply and control systems using silicon-controlled rectifiers (SCRs) are being considered, and will be tested and evaluated. The dc system (using phase-angle-fired SCRs) will have the capability to supply the bundle heaters with continuously variable dc voltage. The ac systems (using zero-voltage firing) in general will control heater power with a combination of switching various values of transformer secondary voltage into the test bundle heater circuits and proportioning the time the heaters are connected to the power source.

3.6.4.1 AC Power and Control Systems

A linear multipower level (LMPL) and a linear multivoltage level (LMVL) scheme for controlling the ac power to the FPS bundle are to be tested and evaluated. Both analog and digital techniques for controlling the SCRs have been studied.

3.6.4.1.1 Analog Technique. The time proportioning control for the FFM, which uses analog techniques, was studied first. Some disadvantages to the CFTL of this control are:

1. The minimum power step is 5%.
2. SCRs provide 1/2-cycle power. The difference in the number of positive and negative 1/2 cycles of ac current produces a dc component. If the dc component exceeds about 2% of the ac current, the transformer core will saturate and the transformer will overheat.
3. The power level drifts randomly from 1 to 2%.

To overcome the disadvantages of the FFM control, an improved time proportioning control, again using analog techniques, was designed, and a breadboard model of it was built. Some features of this control are:

1. The minimum power step is 1.67%.
2. SCRs are fired for full cycles of power.
3. The control system is timed to the 60-Hz power frequency, which eliminates the random drift of the power levels.

The wave forms of the power provided to the test bundle with this control are presented in Fig. 3.19. The results of calculations made to show the effect of this time proportioning control on the temperature of the outside surface of the heater cladding are shown in Fig. 3.20. The maximum variation in surface temperature occurs at 50% power and is further increased as the control period is increased in length. This analog technique has been abandoned in favor of the digital technique.

3.6.4.1.2 Digital Technique. The analog techniques are hampered by several factors. First, the amplifiers are subject to drift and offset and are sensitive to temperature changes and component aging. Second, they typically require a number of adjustments over a period of time to achieve a desired power level. Power is usually determined by counting the number of power pulses delivered to the test bundle over a relatively long period of time (10 sec); then appropriate adjustments are made to the control and the process is reiterated until the required power is obtained.

Fig. 3.19. Wave Forms Produced at Heater by AC Power Supply Using Analog Control with 10% Control Stages.

Fig. 3.20. Variation in Heater Sheath Temperature with Power-On Time at 100% CFTL Heater Power with 60-Hz AC Supply for Various Control Periods with the Analog Control System Used.

To overcome these problems a digital control was designed and a prototype was built. The wave forms of the power provided to the test bundle with this control for 10% power steps are presented in Fig. 3.21.

Some features of this control are:

1. Power control in 1% increments can be achieved.
2. SCRs are fired for full cycles of power.
3. Power settings are reproducible because there is a one-to-one correspondence between the thumbwheel switch setting and the output power.
4. The thumbwheel switches, which visually indicate the power level, provide a very accurate measurement of steady-state power.
5. Interfacing with digital computers and other digital equipment is quite simple. A digital binary coded decimal (BCD) word may be tied directly into the controller to permit control from an appropriate programmer.
6. The control frequency associated with a 1/6-sec period varies from 6 Hz at 90% to 36 Hz at 50% power, as compared with a constant 6 Hz frequency for the analog control with 1/6-sec period.

The variation of the heater surface temperature with power-on-time is plotted in Fig. 3.22. When this plot is compared with the 1/6-sec period plot in Fig. 3.20, it shows that for the 50% power the sheath temperature variation has been decreased from 2.7 to 0.1°C (4.8 to 0.2°F) and at powers of 10% and below and 90% and above the variations are essentially the same. However, to achieve 1% increments of power control with the digital technique requires 100 cycles of 60 Hz power. This results in a predominant 0.6-Hz control frequency above 90% and below 10% power. Another method to achieve 1% incremental power control is the application of an induction regulator into the primary of the supply transformer to each of the individual control zones. The regulator or similar equipment will be a necessary adjunct in any ac power control scheme to obtain the settings for the individual zone power levels in preparation for test bundle transients with skewed power distributions.

3.6.4.1.3 Linear Multipower Level. In the linear multipower level scheme the zone transformer secondary consists of a single winding with

Fig. 3.21. Wave Forms Produced at Heater by AC Power Supply Using Digital Control with 10% Control Stages.

Fig. 3.22. Variation in Heater Sheath Temperature with Power-On Time at 100% CFTL Heater Power with 60-Hz AC Supply Using the Digital Control System.

taps to provide linear power steps between the taps. Both a single- and a five-power-level system will be tested, and the SCRs will be controlled during steady-state operation by the digital control system and during transient operation by a function generator whose functions replace the functions of the BCD switches. A single power level with the digital control system meets the steady-state requirements in Table 3.7. The main advantage of the five-power-level system is that the lowest control frequency is 30 Hz during a power transient, as shown in Table 3.8. If the number of power levels is increased to ten, the lowest control frequency is still 30 Hz for the 1-, 2-, and 4-sec transients, but the number of power steps has been increased from 9 to 18. For steady-state operation, the five-power level system provides 0 to 100% power control in 0.2% power steps.

Table 3.8. Summary of Transient Capabilities of AC Power Supplies

Power or Voltage Levels	Pairs of SCRs	Maximum Number of Power Steps for 100%-to-10% Power Transient for Each Transient Time and Control Frequency									
		1 sec		2 sec		4 sec		16 sec		10 min	
		6 Hz	30 Hz	6 Hz	30 Hz	6 Hz	30 Hz	6 Hz	30 Hz	6 Hz	30 Hz
		<u>Linear Multipower Level</u>									
1	1	7 ^a	2	9 ^a	2	9 ^a	2	9 ^a	2	90 ^a	2
5	5	7	9	9	9	9	9	9	9	360	9
10	10	7	18	13	18	18	18	18	18	360	18
15	15	7	27	13	27	27	27	27	27	360	27
<u>Linear Multivoltage Level^b</u>											
4	16	27		27		27		27		27	

^aControl frequency is 30 Hz or less; other power steps are 30 Hz or 60 Hz.

^bControl frequency is 60 Hz. Additional power steps can be obtained by time-proportional switching between the two closest voltage levels.

3.6.4.1.4 Linear Multivoltage Level. In the linear multivoltage level scheme to be tested, the zone transformer secondary consists of four separate windings, with the voltage selected to provide 40 linear voltage steps as the four windings are added, subtracted, or removed in a programmable fashion with a microprocessor. The largest power step is 4.94%, which occurs just below full power, and the smallest power step is 0.063%, which occurs just above zero-power transient. The power steps during a 100%-to-10% transient vary from 4.94% for the first step to 1.56% for the 27th step. If required, additional power steps can be obtained by time-proportional switching between the two closest voltage levels.

3.6.4.1.5 Transient Capabilities. A summary of the transient capabilities of the linear multipower level and linear multivoltage level supplies is shown in Table 3.8. Heater sheath temperature has been calculated as a function of step changes in heater power for the CFTL heaters for four and nine equal power steps from 100% to 10% power. The sheath temperature is plotted against time for the 1-sec transient and nine equal power steps in Fig. 3.23. The plot is representative of the 1-sec transient for a five-power-level linear multipower system.

Fig. 3.23. Heater Sheath Temperature vs Time for a Power Transient from 100% Rated Power to 10% Power in Nine Equal Steps over an Interval of 1 sec.

3.6.4.2 DC Power and Control System

The difficulties of matching the characteristics of the large dc motor-generator sets already installed at ORNL to the power requirements for the CFTL test bundles encouraged us to look into the rectification of ac power with SCR equipment. We were also encouraged by news of plans to use rectified dc power in the Semi-Scale Facility at Idaho Nuclear Engineering Laboratory and in the Controlled Thermonuclear Research program at the Laboratory. In this scheme, the SCRs are triggered to conduct at various voltage phase angles and thus to produce an output voltage that is an average of the conduction portion of the wave form. The voltage phase angle for firing the SCRs may be varied to give a very accurate and continuous control of the output dc voltage. However, phase-angle firing can produce steep wavefronts, which generate a large amount of noise in both the power supply and output. By employing polyphase systems such as 6 or 12 phases, both ripple and noise are reduced in size and intensity and elevated to frequencies that permit convenient and reliable filtering of the output power.

The dc power and control should meet all the controlled power requirements for CFTL test bundles as shown in Tables 3.6 and 3.7. To evaluate the amount of noise in the input of the dc power supply and the effects of the electromagnetic interference noise (emi) on the FPS thermocouple signal, a 60-kW dc power supply (six phase) is being purchased (delivery scheduled July 15) with the capability for testing the low-power heaters in the seven-rod FPS test bundle and also testing a single prototypic FPS at full power and temperature in the FPS Test Facility.

3.6.4.3 Transfer Function Test

The dc power supply employing phase-fired SCRs being considered produces in the output considerable amounts of noise in the form of harmonics of 60 Hz. The effect that this noise has on the heater internal thermocouple signal is difficult to determine unless the degree of electromagnetic coupling between the heater element and the thermocouple leads is known. Tests are to be conducted to determine the degree of

coupling as a function of frequency. With the data that are expected to be obtained from the test it will be possible to compute the noise appearing on the thermocouple lines for a given configuration of power supply and filter.

The maintenance on an available 30-kW, 400-Hz generator was completed, and the generator has been reinstalled. It will be used to determine the heater-to-thermocouple transfer function in the 60 to 400 Hz range. A variable-frequency oscillator supply has been checked for possible use in measuring the transfer function. The output of this supply is limited to about 50 V and 4 A. By using a 4- to 1- step-down transformer, 10 A at 1080 Hz has been obtained when the supply was connected to a 1- Ω load, which is approximately the electrical resistance of a CFTL test bundle heater. By the proper selection or design of a step-down transformer, it may be possible to extend the useful frequency range of the transfer function test.

3.6.4.4 Seven-Rod Heater Test

The seven-rod FPS test bundle is being built to test and evaluate the ac and dc power supplies. The assembly for holding the simulators which has been fabricated, has the same FPS spacing as the CFTL bundle. The assembly will be installed in a water tank to provide adequate cooling. Three different types of thermocouple installations will be provided inside each FPS. One is an intrinsic thermocouple consisting of two individually sheathed 0.38-mm-OD (0.015-in.) type K thermoelements, each of which is spot-welded to the inside surface of the heater sheath. The other two thermocouples will be sheathed 0.51-mm-OD (0.020-in.) type K thermocouples, one with a grounded junction and the other with an insulated junction.

The FPS configuration will be similar to that for the prototypic FPS, except that all current-carrying components (including the coiled heating element) will be fabricated from copper, and magnesium oxide insulation will be used in lieu of boron nitride (BN). These low-power heaters will produce about 750 W when operating at 160 A (rated current of CFTL heaters) and 4.6 V.

A patch panel cabinet has been designed and built to allow the seven heaters to be connected in various series and parallel combinations such that the worse noise condition can be obtained. If possible, we will determine how much of the thermocouple noise is a result of the current on the center heater, and how much is caused by the surrounding heaters.

The design of the linear multipower-level and linear multivoltage-level ac power systems has been completed, and much of the equipment has been fabricated. The initial test with a single-power-level system is scheduled to start by July 1, 1975.

The following is a summary of the tests planned for the seven-rod FPS bundle:

1. determine transfer function of heater and thermocouples;
2. characterize the steady-state and transient performance of the ac and dc power supplies;
3. determine input noise to thermocouples from the heaters for both steady-state and transient conditions;
4. establish filtering techniques and capabilities.

3.6.4.5 High Temperature and Power Test

Tests similar to those planned for the single-FPS test in the seven-rod FPS bundle will be conducted on a single prototypic CFTL simulator in the FPS Test Facility. The seven-rod FPS test bundle ac power supplies will be modified by replacing the zone transformer and the primary Powerstat for the higher current and voltage rating. No modification of the 60-kW dc power supply is required. Because of the low power and temperature rating of the heaters in the seven-rod bundle, the full-power testing of the candidate power supply and controls will be conducted in the FPS Test Facility.

3.6.4.6 Heater Protection

Each test bundle FPS will be protected with a current-limiting fuse. We are studying additional protection for each row of FPSs with a ground fault interrupter (GFI) or similar device to detect small leakage

currents by monitoring the difference in the value of the currents in the two power supply leads for each row of FPSs. A preliminary design of an ac and a dc leakage current detector has been prepared, and a breadboard assembly has been built and is being evaluated.

3.7 INSTRUMENTATION AND CONTROLS — H. J. Metz

The early part of fiscal year 1975 marked the beginning of the involvement of the Instrumentation and Controls Division in the GCFR Core Flow Test Loop (CFTL). As soon as the conceptual loop design was available, measurement and control flow diagrams were prepared to make cost estimates of measurements and controls for the loop. Later on, the CFTL Program Plan revealed requirements for measurements of test bundle parameters, such as strain, motion, and vibration, that would require research and development. Also the quantity of test bundle measurements was large. In subsequent discussions between GA and ORNL, the requirements for measurements that needed development were eliminated. Also, the quantity of test bundle measurements was reduced to decrease the size of the high-speed data acquisition system (HSDAS).

Study of the conditions to which the test bundles would be subjected, the design of the electric heaters that will simulate the GCFR fuel pins, and the electrical power supply and control to the test bundle rods indicated a high probability that electrical noise picked up by thermocouples installed inside individual FPSs would make it difficult or impossible to obtain accurate data, particularly during the fast transients. Investigations revealed a serious noise problem with ac power, which probably will require filtering. The need for fast response of the sheath temperature thermocouples makes filtering somewhat difficult when the frequency is less than 30 to 60 Hz. This difficulty directed our attention to the test bundle power supply and control and the linear multivoltage level scheme (LMVL) described in Sect. 3.6.4.

The need to control five variables during the fast transients led to the development of a transient controller.

The preliminary specification of an HSDAS was proposed.

Because of concern about the accuracy and durability of the small-diameter thermocouple that will be installed in the FPS to measure sheath temperatures, tests were made to determine the viability of the design.

A gas chromatograph for analyzing the CFTL helium was investigated. The neon analysis, which would be very troublesome, was deleted from the requirements by mutual agreement with GA. Because of the different gases and the water vapor to be measured and the range of measurement, the chromatograph will be a rather complex continuous process instrument having two columns and two samplers.

3.7.1 Coolant Helium Flow Measurement — H. J. Metz

The measurement of coolant helium flow rates [2.81-0.045 kg/sec (6.3-0.1 lb/sec)] must accommodate a flow ratio of 63. This results in a required maximum-to-minimum differential pressure ratio of 3969 for velocity head flowmeters. Velocity head meters have a maximum flow ratio of approximately ten. Therefore, several different sized velocity head meters are required to cover the CFTL flow ratio. Initially, venturi tubes were selected to measure steady-state test bundle and attenuator flow rates and total flow rate from the circulators. However, the response of venturi tubes, together with the associated piping and differential-pressure transmitters, is too slow to measure transient flows accurately. This would not allow test conditions to be verified with sufficient accuracy to meet the requirements of the Program Plan. We found that target flowmeters, which also have a flow ratio of approximately ten, would have fast enough response to measure the transient flows.

The use of venturi tubes and target flowmeters at each of the three flow measuring stations would require four venturi tubes and two or three target flowmeters for each station. The most troublesome part of the use of these velocity head flowmeters is the need to change meter size each time the test bundle size is changed.

Additional possible methods of flow measurement were investigated. Ultrasonic flowmeters promise accuracy at a flow ratio of 75, along with

high-speed response and the capability to sustain CFTL operating conditions. A prototype of the ultrasonic flowmeter is being purchased. We plan to test and evaluate this flowmeter either in air in collaboration with the Colorado Engineering and Experiment Station near Nunn, Colorado, or in helium at the U.S. Bureau of Mines Helium Facility, Amarillo, Texas.

3.7.2 Helium Flow Control Valves — H. J. Metz

Fast-acting valves will be needed to perform the necessary coolant helium flow transients for test bundle and circulator bypass flows. Other similar controls will be required for the cooling air flow to the two helium-to-air heat exchangers.

The valve coefficients and the ratio of maximum-to-minimum valve coefficients (rangeability) for each test bundle were calculated. The largest pressure loss used in the calculations for fully open valves was 62 kPa (9 psi). At 10% flow the pressure drop used in the calculations was the circulator head minus the pressure loss in the test bundle and in the loop. The results of the calculations are given in Table 3.9.

Table 3.9. Test Bundle Valve Requirements

Number of Heaters	For 100% Flow			For 10% Flow			Rangeability Required
	C _v , Valve Coefficient ^a	Flow Rate		C _v , Valve Coefficient	Flow Rate		
		(kg/sec)	(lb/sec)		(kg/sec)	(lb/sec)	
91	174	2.77	6.1	6.4	0.277	0.61	27
61	117	1.86	4.1	4.7	0.186	0.41	25
37	71.2	1.13	2.5	3.2	0.113	0.25	22
19	36	0.57	1.25	2.0	0.057	0.125	18
127 ^b	36	0.56	1.23	2.0	0.056	0.123	18

^aDefined by $C_v = \frac{Q}{A} \left[\frac{(SG)T}{\Delta P(P_i + P_o)} \right]^{1/2}$, where Q = flow through the valve in std liters/sec scfh, (SG) is the specific gravity of the gas relative to air at 15.6°C (60°F) and 0.101 MPa (14.7 psia), T is the temperature in kelvins or degrees Rankine, and ΔP , P_i , and P_o are, respectively, the pressure drop, inlet pressure, and outlet pressure in pascals or psia. The constant A is 8.20×10^5 for the indicated metric units and 963 for the English units.

^b4.41 g/sec (35 lb/hr) per blanket heater.

The rangeability requirements to control the flow transients for various combinations of test bundles and the characteristics of a commercially available valve are compared in Table 3.10. The maximum valve coefficient of 174 and the minimum of 2 yield a rangeability of 87, which is deemed to be too large for a single valve with only one set of trim.

The commercially available valve having a maximum valve coefficient of 195 and used over a rangeability of 60, which is thought to be a reasonable value, will provide flow transient control for both the 91- and 61-heater test bundles. The same valve with a special set of trim installed having a maximum valve coefficient of 75 and a rangeability of 60 will control the flow transients in the 37- and 19-heater bundles and the 127-heater blanket bundle. We favor this valve and the two sets of trim.

Table 3.10. Valve Requirements and Characteristics

Range of Numbers of Heaters	<u>C_v, Valve Coefficient</u>		Rangeability
	Maximum	Minimum	
<u>Combined Requirements</u>			
91	174	6.4	27
91 and 61	174	4.7	37
91-37	174	3.2	54
91-19	174	2.0	87
<u>Characteristics of Commercial Valve</u>			
91 and 61	195	3.3	60
37 and 19	75	1.25	60

The speed of valve stem movement for the fast flow transients specified by GA will require the use of hydraulic valve operators having a spool valve actuated by an electromechanical valve. The electro-mechanical valve will receive an electrical signal from a servo amplifier. The valve stem position will be fed to the servo amplifier so that the

desired valve stem position and the actual valve stem position can be compared. The signal to the electromechanical valve will be changed as necessary by the servo amplifier to make the actual and desired valve stem positions coincide. The control signal to the servo amplifier will come from the transient controller.

Specifications have been written for the helium flow control valves, the hydraulic fluid, and the 20.7-MPa (3000-psi) hydraulic fluid supply system.

3.7.3 Transient Controller System - M. J. Roberts and W. E. Miller

The transient operation of all proposed test bundle power supplies; the coolant helium valves that control flow to the test bundle, the attenuation path, and the circulator bypass; and the valves that control cooling air flow to the heat exchangers will be controlled with a programmable read-only memory (PROM) function generator (PFG). This system will be programmed to produce the several transients and repetitive cycles required for the CFTL. The PFG has undergone three stages of development. The latest of these is now being fabricated to provide the control functions for the CFTL seven-heater water test.

The PFG is block-diagrammed in Fig. 3.24. An electronic clock, whose frequency is selectable by the user, increments a binary counter. The 8-bit binary number from the counter consecutively addresses the memory locations as it is incremented. As the address is incremented, the 8-bit binary data from the PROM change according to the function stored in the PROM. These data can be used in binary form or converted to an analog voltage or current to be used as a control set point for a process.

3.7.4 High-Speed Data Acquisition System - C. D. Martin, J. L. Redford, and H. J. Metz

The maximum frequency during transients for nearly all test section measurements as indicated in the GA Program Plan is one per second except that bundle power measurements are taken ten times per second during a transient. However, in our opinion, verification that a CFTL fast transient duplicates somewhat faithfully the data indicated in GA's,

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Fig. 3.24. Programmable Read-Only Memory Function Generator for Controlling Transient Operation of CPTL.

"Transient Data for GCFR, Gravity and Shutdown Data from MAXGAF Calculations," will require many measurements of several important parameters to be made during the transient. These measurements include tentatively test bundle electrical power, helium flow rate through the test bundle, test bundle inlet and outlet helium pressures and temperatures, test bundle differential pressure, and heater sheath temperatures. An additional complication to the control and measurement of test bundle power is the lower specific heat of the bundle simulators compared with a GCFR fuel rod. This will require modifying bundle power input from the values for GCFR core power shown in the MAXGAF tables to attain on the CFTL heater cladding the same temperature values the GCFR rod cladding experiences, particularly during fast transients. Verification of the heater cladding temperatures will require a number of measurements per second during transients.

Frequent and accurate measurements taken during the transients will permit both GA and ORNL to assure the USNRC, and also possible intervenors, that the CFTL test conditions indeed do duplicate these anticipated with the GCFR.

Figure 3.25 shows a block diagram of the High-Speed Data Acquisition System (HSDAS) that has been selected tentatively for the CFTL. It will accept 640 input channels of measurements and can be increased to 1024 channels by installation of additional 128-channel modules. During fast transients the HSDAS can record data on magnetic tape at the rate of 20,000 channels per second continuously for up to 200 sec and provide simultaneous limit checking for alarms and shutdown initiation. During steady-state and slow-transient test operations, all signals will be recorded on magnetic tape and limit checked for alarm and shutdown initiation periodically, and the HSDAS will also provide on-line real-time calculation and display of some system operating conditions. The computer used in the HSDAS will be able to provide the experimenter with a comprehensive analysis of previously acquired data in an off-line mode of computer operation.

We investigated methods of measuring either ac or dc test bundle power. The most feasible method appears to be the use of shunts to

Fig. 3.25. Block Diagram of High-Speed Data Acquisition System for CFTL.

measure current and the utilization of the computer to calculate power from voltage and shunt output measurements.

3.7.5 Studies on Insulation Effects in Small-Diameter Thermocouple Material — R. L. Anderson and T. G. Kollie

Small-diameter 0.51 mm (0.020 in.) sheathed thermocouple materials are used in simulation tests of reactor fuel rods for both the LMFBR and the GCFR. The stringent requirements on the temperature measurements for these tests and the relatively recent availability of small thermocouple materials indicated a need to measure the insulation properties of these materials, particularly considering the small diameter of the materials and the relatively high temperatures involved. In the course of the investigations, a number of effects were investigated related to the reliability of small-diameter thermocouples.

A fairly "typical" example of the insulation resistance (IR) versus temperature is shown in Fig. 3.26. "Typical" in this case carries a number of qualifications. This curve may be produced only after several temperature cycles. For example, Fig. 3.27 shows the data taken on a 0.51-mm-diam (0.020-in.) thermocouple beginning with its "as received" condition through four thermal cycles. On the first heating cycle, the IR sharply decreases at about 100°C. The same effect, see Fig. 3.28, has been observed in larger diameter materials by Horton et al.¹ and is believed to be due to release of the water absorbed on the MgO insulation.

Fig. 3.26. Typical Insulation Resistance vs Temperature for Small-Diameter Sheathed Thermocouple Material.

Fig. 3.27. Insulation Resistance Versus Temperature for 0.51-mm-diam (0.020 in.) Thermocouple Material Beginning with As-Received Condition Through Four Heating and Cooling Cycles. Curves 1 through 4 represent these cycles; only the cooling portion is shown for cycles 3 and 4. Curve 5 shows similar material with the hot end seal removed to ease escape of water vapor. High-temperature portions of curves are shown for larger diameter material.

Fig. 3.28. Insulator Resistance vs Temperature for Nine 0.125-in.-OD Specimens.

Figure 3.29 shows the effect of temperature on the adsorption of water on MgO. High-temperature processing of the insulation material is obviously necessary to remove the absorbed water, which is most likely not done in the manufacture of these materials. When one zone of thermocouple material is heated to high temperatures (up to 1000°C), the adsorbed water in the heated zone will be released and diffuse to the cooler portions near the ends of the furnace. This process can be observed in Fig. 3.27 in going from heating cycles 1 through 4. The IR represented by curve 4 is fairly stable and reproducible, provided the temperature gradient is not shifted along the thermocouple. If a fresh previously unheated section of the thermocouple is moved into the furnace, then the IR again behaves like the first heating in curve 1.

If the end seal of the thermocouple in the hot zone is cut off so that the water vapor is free to escape, a much higher IR can be obtained, as shown in curve 5 on Fig. 3.27. The droop at the cold end shows that as the temperature of the system falls below about 100°C, water begins

Fig. 3.29. Residual Adsorbed Water on MgO After Prolonged Pumping at Various Temperatures. Based on P. J. Anderson and P. L. Morgan, *Trans. Faraday Soc.* 60: 930-37 (1964).

to readsorb and lower the IR. Also plotted in Fig. 3.27 for comparison are the high-temperature portions of the measured IR of materials with diameters of 1.59 and 3.17 mm (1/16 and 1/8 in.).

There is some evidence that the value of the IR is voltage dependent, so that the values measured typically at 10 or 50 V may be quite different from an effective IR for thermocouple voltages in the millivolt range. This effect has not been extensively investigated.

Experiments at higher temperatures and with larger diameter materials have shown serious errors (up to 400°C) because of insulation shunting and establishment of "virtual junctions" when portions of the thermocouple assembly are at higher temperatures than the measuring junction. The experiment shown in Fig. 3.30 was set up to check this effect in small-diameter material. Two 0.51-mm-diam (0.020-in.) thermocouples were placed in a furnace with their junctions tied together. A 1-m portion of one thermocouple was coiled and run through a second furnace, while the second thermocouple was left at room temperature outside the second

Fig. 3.30. Schematic of Experiment to Measure Error Resulting from Establishment of "Virtual Junction" Caused by Insulation Shunting When Portions of Thermocouple Assembly Are At Higher Temperature Than the Measuring Junction.

furnace. The two thermocouples were connected together differentially and then connected to the y axis of an x - y recorder. A third thermocouple was placed in the second furnace to read the temperature of the hot zone and connected to the x axis. With the test thermocouple junction at various temperatures in the first furnace, the hot-zone temperature in furnace 2 was varied, and the results were plotted by the recorder. These results are shown in Fig. 3.31. The results indicate a maximum potential error up to 36°C with the second furnace at 1100°C and the first at room temperature.

An additional effort was made to elucidate this phenomenon with the experimental setup shown in Fig. 3.32. Two 0.51-mm-diam (0.020-in.) type K sheathed thermocouples were mounted inside a quartz tube installed inside a furnace, one thermocouple entering from either end of the furnace. About 80 mm (3 in.) of the ends of each thermocouple was coiled, and the junctions were tied together with a ball of platinum wire. Two type S (Pt-10% Rh vs Pt) thermocouples insulated with alumina beads were also attached to the platinum ball. As with the type K thermocouples, one type S couple was led out either end of the furnace. The thermocouple assembly was sealed in a quartz tube, which had been purged with flowing helium to reduce the oxidation of the thin stainless steel sheaths at elevated temperatures.

Fig. 3.31. Thermocouple Error Caused by Heating Portion of Thermocouple Assembly with Thermocouple Junction Held at Several Different Constant Temperatures.

Fig. 3.32. Experimental Arrangement for Measuring Thermocouple Readout Error Due to Position in Furnace. Two 0.51-mm (0.020-in.) type K sheathed test thermocouples and two type S thermocouples insulated with alumina beads are attached to a platinum ball, and the thermocouple junction region is sealed in a quartz tube. The quartz tube is then slid in and out to scan the temperature variation in the furnace.

The results are shown in Fig. 3.33. The curves are the differences indicated by the type K thermocouple and the average of the two type S thermocouples. The two type S thermocouples agreed with each other within about 2°C throughout the experiment. The furnace was scanned by moving the thermocouple assembly in the quartz tube right or left by measured amounts, so that the thermocouples were placed in various parts of the temperature gradients in the furnace. At 600°C, the two type K thermocouples agreed to within 4°C at all positions in the furnace. At 800°C, however, one of the thermocouples began to show large ($\pm 6^\circ\text{C}$) deviations from the temperature measured by the type S thermocouples. In Fig. 3.33(c) the results of two scans made at 1000°C indicate that deviations are larger (up to 30°C) than at 800°C and much greater in the second run than in the first, indicating time dependence also.

After completion of the experiment in the furnace, the thermocouples were disassembled and tested in the Instrumentation and Controls Division homogeneity test rig. The results in Fig. 3.34 show that the high temperatures caused significant changes in the thermoelectric materials themselves, with about twice the effect occurring in the Chromel leg. We believe that the effect is due to the oxidation of the very small [0.076-0.127 mm-diam (0.003-0.005-in.)] wires, since a similar test on 1.59-mm-diam (1/16-in.) materials showed little effect.

This experiment shows that the large errors can occur when the temperature gradient changes along the thermocouple after it has seen high-temperature service. With the assembly in the center of the furnace, the type K and the type S thermocouples agree fairly well.

These studies have indicated that the small-diameter type K thermocouples can be considered fairly reliable up to 600°C if certain precautions are taken. To extend the range of these sensors, a proposition of doubtful merit considering the inadequacies of the Chromel-Alumel materials themselves, further studies will have to be conducted on ways to reduce the development of the large inhomogeneities in the temperature gradients. The shunting due to the decreasing IR does not appear to present a serious problem even up to 1000°C for relatively modest loop resistances. As the loop resistance of the thermocouples become large, the IR shunting can again become a problem.

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Fig. 3.33. Differences in Temperatures Indicated by Individual Type K Thermocouples and Averages from Two Pt vs Pt-10% Rh Thermocouples as Measured in the Experiment Shown in Fig. 3.32. The horizontal axis represents furnace positions as shown in Fig. 3.32. (a) First scan at 600°C. (b) First scan at 800°C. (c) First scan at 1000°C and second scan 24 hr later.

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Fig. 3.34. Inhomogeneity Test Conducted on East Thermocouple After Completion of Tests in Experimental Arrangement Shown in Fig. 3.32.

3.7.6 Thermocouple Noise in Alternating Magnetic Fields — D. W. McDonald and M. J. Roberts

We undertook an investigation into the effects upon thermocouple readings of the electromagnetic interference (emi) generated by a candidate CFTL power supply. The results indicated that emi would not be a problem, and that if the lowest frequency output of the power supply is near 60 Hz, with filtering and a carefully designed grounding system the effects of the interference will be minimal. However, without filtering the output of thermocouples that use ferromagnetic thermoelements will be contaminated with a series mode ac noise voltage having an amplitude proportional to the magnitude of the ac current of the power supply and inversely related to the temperature of the thermocouple, and a lowest frequency component equal to the lowest frequency of the power supply. The ac noise voltage wave form consists of sharp spikes. The amplitude of these spikes may attain levels equivalent to a 27.8°C (50°F) error for a 60-Hz power supply and 126°C (227°F) error for a phase-fired power supply if the spike and data system input coincide. However, since the spikes are very narrow and the data system and spikes will coincide randomly for an individual thermocouple, an average or rms value better indicates the actual experienced error at the output of a measuring device due to this noise. An rms error of 0.34°C (0.7°F) for the former and a rms error of 1.7°C (3°F) for the latter are more typical of the error one may expect. These seemingly small errors must be considered significant since 1.7°C is 20% of the total error allowed, and other errors due to the thermocouple properties and the CFTL data acquisition system have not yet been fully analyzed. With filtering, these measured errors can be reduced significantly.

We measured emi noise in a mockup of a CFTL heater rod. The experimental setup for measuring the noise due to the electric field is shown in Fig. 3.35. The thermocouples tested include 0.51-mm-OD (0.020-in.) stainless-steel-sheathed magnesium-oxide-insulated Chromel vs Alumel thermocouples, 0.38-mm-OD (0.015-in.) individually sheathed Chromel and Alumel wires, and 0.51-mm-OD (0.020-in.) stainless-steel-sheathed Nisil vs Nicrosil thermocouples. The voltage between the heater element and the heater sheath was varied from zero to over 130 V. This variation

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Fig. 3.35. Measurement of Noise Induced in a Thermocouple by an Alternating Electric Field.

caused an alternating electric field across the thermocouples. No noise could be detected at the thermocouple output because the type 304 stainless steel sheaths provide a good shield for electric fields.

The experimental setup for measuring the noise due to the magnetic field is shown in Fig. 3.36. The same thermocouples were used in both tests. The alternating current through the heater element was varied from zero to over 125 A rms. This caused the thermocouples to be in an alternating magnetic field. This resulted in only slight noise at the output of the Nisil vs Microsil thermocouple, approximately 10 μ V rms, which is equivalent to an instantaneous error of 0.17°C (0.31°F). Both types of Chromel vs Alumel thermocouples produced a large-amplitude noise, approximately 1 mV, which is equivalent to 17°C (31°F) error at the peak of the noise spikes. Continued investigation revealed that interaction between the ferromagnetic Alumel and an alternating magnetic field generates a series-mode noise voltage. The amplitude is directly related to the amplitude of the current inducing the magnetic field and inversely related to the thermoelement temperature. The amplitude approaches zero as the temperature approaches the Curie temperature of the thermoelement, which is 134°C (364°F). The noise voltage from Nisil-Microsil was negligible because the Curie temperature of Nisil is 25°C (77°F). Noise was generated in Nisil when it was cooled below the Curie temperature.

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Fig. 3.36. Measurement of Noise Induced in a Thermocouple by an Alternating Magnetic Field.

The amplitude of this "magnetic effect" is extremely sensitive to spatial orientation. A noise output of 707 μV was decreased to 56 μV by rotating the thermoelement slightly. The output of the 0.38-mm (0.015-in.) individually sheathed thermoelement Chromel vs Alumel thermocouple revealed 60-Hz pickup as well as the "magnetic effect" noise. Because the individual thermoelements were not close together, the loop thus formed was picking up the 60-Hz magnetic field. The experiment was reassembled with the distance between the thermoelements kept to the minimum. Then the noise pickup was almost eliminated.

In all the magnetic field tests, magnetic shielding material was wrapped around the thermocouple outside the heater sheath to eliminate extraneous effects. Without the shielding, the "magnetic effect" remains even though the thermoelement within the heater is above the Curie temperature. The magnetic field exists outside the heater and interacts with the Alumel, which is at room temperature outside the heater.

A 90° phase-fired current from a silicon controlled rectifier (SCR) was fed to the heater. Below the Curie temperature the thermocouple outputs were extremely noisy. Voltage spikes were over 5 mV, which is equivalent to an instantaneous error of 126°C (227°F). Above the Curie temperature, the noise became an insignificant 0.64 μV rms, equivalent

to 0.36°C (0.64°F). These small spikes were due to residual magnetism above the Curie temperature and did not decrease with large temperature increases.

Passive, active, and digital filters are being investigated. Several breadboarded schemes are being compared for stability, cost, and size factors. Commercial filters were also investigated and rejected as undesirable for the specific CFTL application. The investigation presently centers on several combined active filters and low-noise preamplifier design schemes.

3.7.7 Power Supplies for the CFTL Test Bundles — M. J. Roberts and R. W. Rochelle

The two areas of power supply type and the filtering methods for thermocouple signals interact strongly because the frequency of the noise picked up depends on the frequencies of the power supply. As the lowest frequency of the power supply becomes smaller, the more difficult it is to filter out noise when fast transients must be recorded. Electromagnetic interference frequencies as low as 2 Hz cannot be removed by filters in a fast-response system.

For a dc heater power supply, the path for interference with the thermocouple output signal is through the conduction of the insulation between the heater and the thermocouple wires. Insulation at CFTL test conditions will be a relatively good conductor, and there will be large leakage currents inside the heater sheath that will take the most conductive paths. It is believed that the chances of the millivolt thermocouple signals surviving uncontaminated by a high dc heater current is quite small. There would be no way to know how much of the thermocouple output was caused by leakage and how much by the hot junction unless the heaters were turned off and isolated from the temperature measuring system. Therefore the use of dc power supplies is questionable at this time.

When ac power to the heaters is used the conductance of the insulation is not as serious a problem as for dc power if the leakage currents have a frequency of about 60 Hz. The 60-Hz noise can be removed by filtering. The thermocouple response to the heater power transients

where power is reduced from 100% to 10% in about 1 sec is in the frequency band from 0 to 10 Hz. A filter that rejects 60 Hz combined with a filter that will conduct everything below 10 Hz will remove the noise from the thermocouple signals caused by ac leakage currents.

The ac power to the heaters can be smoothly controlled to supply the required power profile for the transient tests. This method would give the least thermocouple noise problem. Power can be supplied by "zero-phase-fired" SCRs. The normal technique is to delete cycles to reduce power. A one-cycle-on and nine-cycles-off heater power control mode will have a strong component at 6 Hz. This frequency will be passed through the thermocouple signal filter and could cause errors. Another method is to use "phase-fired" SCRs. The frequency spectrum will contain 60 Hz and higher frequencies. Little energy will go through the thermocouple signal filter. The steep-sided transients produced by the "turn on" of the SCR contain powerful high-frequency components, which pass around filters and are difficult and expensive to remove.

There are some ac power supply designs that should reduce thermocouple noise pickup. Three of these are the switched secondary system, the switched resistance system, and the switched capacitor system. The switched secondary supply transformer secondary windings have relative voltage outputs of 1, 3, 9, and 27 are connected as shown in Fig. 3.37(a). A winding can be added, subtracted, or disconnected to produce an output voltage from zero to 40 to provide 40 power levels. Power level is plotted against step number in Fig. 3.37(b). The largest power step is 5%, and the control becomes more precise as the power is lowered. This system has been operated successfully on a laboratory bench scale.

The switched resistance system is illustrated in Fig. 3.38(a). The four resistors are related to each other by a factor of 2: $R_n = 2R_{n-1}$. The solid curve in Fig. 3.38(b) gives the relation between resistors connected and power delivered to the load. The dotted curve is power taken from the line. Intermediate average values of power are obtained by time-proportioning switching between the two closest levels. The largest power step is 23.6% just below full load. Figure 3.39 shows how control could be improved. The maximum power that is dissipated in the resistances is 25% of full load. It occurs at 10% of full load.

Fig. 3.37. Switched-Transformer-Secondary Power Supply. (a) Wiring diagram. (b) Heater power versus voltage output.

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(a)

(b)

Fig. 3.38. Switched-Resistance Power Supply. (a) Wiring diagram. (b) Heater power and line power versus switch combination.

Fig. 3.39. Optimum Switched Resistor System Performance.

The switched capacitor supply is shown in Fig. 3.40(a). The secondary of the transformer must produce 40% greater voltage. For maximum power all the capacitors are connected in parallel. This capacitance, 15C, has a voltage drop equal to the heater voltage drop. The phase angle is 45% leading. As capacitors are taken out of the circuit, the power falls. Figure 3.40(b) shows this relationship. The maximum power step is 8.7%. When this system was tested in the laboratory, timing problems were encountered.

The switched secondary system has the most advantages because no power is wasted, the kVA rating of the transformer does not have to be increased, and there are no timing problems.

3.8 REFERENCE

1. J. L. Horton, "Thermocouple Insulation Resistance," *Instrumentation and Controls Div. Annu. Progr. Rep. Sept. 1, 1972*, ORNL-4882, p. 66.

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Fig. 3.40. Switched Capacitor Power Supply. (a) Wiring diagram. (b) Heater power versus switch combinations.

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4. GCFR SHIELDING STUDIES

D. E. Bartine

4.1 CALCULATIONS OF THE PRELIMINARY RADIAL SHIELD DESIGN — D. E. Bartine

In this first separate GCFR progress report, work is covered from May 1974 through June 1975. Major areas are presented in chronological order where possible, although funding fluctuations resulted in an off-again-on-again approach to some items. Shielding studies at ORNL are closely coordinated with design work¹ at GA, and progress in this area is therefore the result of efforts of both institutions. Accomplishments to date include the identification of areas for concern in the GCFR shielding model supplied by GA and their incorporation into the GCFR shielding program plan, the development of cross-section sets, and their application in parametric studies to determine desirable computational models for GCFR calculations. These cross sections and modeling approaches were then applied in a cross-section sensitivity study for high-energy flux transmission to the PCRV liner, and to evaluate the effects of proposed revisions to the outer radial shield. Current results indicate that the prospects for reduction of the outer radial shield, and therefore the reactor cavity radius, are reasonably favorable from the shielding point of view.

4.1.1 Assessment of Basic Areas of Concern in the Development of a Shielding Program for a GCFR Conceptual Design

The General Atomic Company provided the two-dimensional cylindrical model used for shielding calculations, which is shown in Fig. 4.1. The core is divided into four zones surrounded by the depleted UO_2 axial and ThO_2 radial blankets. The core is suspended from the grid plate. Shielding is contained in the shaded zones, specifically the inner and outer radial shields, upper axial shields, and the lower axial shield. Special shielding protects the grid plate; concrete containment extends radially beyond the iron liner in the form of the prestressed concrete reactor vessel (PCRVR).

Transport calculations for the GA two-dimensional shielding model were run with DOT² in r - z geometry using the 50-group cross-section

Fig. 4.1. Shielding Model of a 300-MW(e) GCFBR in Cylindrical Geometry.

library employed at ORNL for FFTF studies. Initial calculations were run P_1 and S_6 , and an additional P_1 calculation of the lower outer radial shield "wraparound" region was run with a biased quadrature set containing 306 angles designed to transport the flux below the outer shield and back up to the PCRV liner. Isoflux levels were plotted for energy ranges above 1 MeV, above 0.1 MeV, and below 2.38 eV (thermal) and for the total flux. Isodose and isodamage plots were also obtained, and flux spectra were plotted at several positions of interest. Figure 4.2 shows a total flux isoplot for the r - z shielding model presented in Fig. 4.1, indicating flux levels starting with an inner level of $7 \times 10^{14} \text{ n cm}^{-2} \text{ sec}^{-1}$ in decreasing factors of 10. Initial criteria for allowable flux levels were a high-energy ($>1 \text{ MeV}$) flux level of 2.7×10^9 and a low-energy (<2.38) flux level of $1.0 \times 10^9 \text{ n cm}^{-2} \text{ sec}^{-1}$ at the PCRV iron liner and a fast ($>0.1 \text{ MeV}$) flux level of $1.0 \times 10^{14} \text{ n cm}^{-2} \text{ sec}^{-1}$ at the face of the grid plate. It should be noted that these

Fig. 4.2. Isoplot of the Total Neutron Flux for the r-s 300 MW(e) GCFBR Shielding Model. Isoflux lines are shown for the inner level of $7 \times 10^{14} \text{ n cm}^{-2} \text{ sec}^{-1}$ in decreasing factors of 10.

constraints were adapted from the HTGR program and that constraints specifically applicable to the GCFR are gradually evolving. The constraint levels stated in this report are those that have been used at ORNL during FY 1975, and should be viewed as current indications of desired levels rather than as fixed maximum radiation levels.

Because some discussion centered around the use of fluences to calculate radiation-induced damage in various stainless steel components and in the PCRV liner for the GCFR, 10% residual uniform elongation lifetimes in type 316 stainless steel were calculated for the original GCFR shielding model provided by GA. The resulting lifetime contours are much more constant over the region containing the axial and radial blankets, inner shields, and grid plate than are the isodose and iso-flux curves. This is apparently due to damage build-up from the spectral softening in the blanket and shield zones. A 10% residual elongation lifetime of 4×10^6 sec was calculated for type 316 stainless steel at the face of the grid plate as compared with a lifetime of 9.5×10^6 sec for a fast (>0.1 MeV) fluence limit of 9.5×10^{21} n/cm².

The basic areas of concern identified in this preliminary study include transport to the PCRV liner through the thorium oxide radial blanket and radial shield and below the outer radial shield and streaming through the outlet coolant duct. The calculated high-energy flux levels at the grid plate face were marginally acceptable. However, since these calculations used a homogeneous representation of the core and blanket instead of the designed fuel rods and fuel rod cell assemblies, and since the type 316 stainless steel damage indicated halving the grid plate lifetime, streaming from the core to the grid plate face was also identified as an area of concern. This study was reported as part of a joint ORNL-GA presentation.³

4.1.2 Production of a 51-25 Coupled Neutron-Gamma Cross-Section Library

An effort was initiated to create a full set of cross sections for GCFR applications from the best ENDF data available. The 51-25 coupled neutron-gamma energy group structure used for FTR and LMFBR Demonstration Reactor calculations was selected so that cross-section sets developed for those programs could be applied to the GCFR. The cross-section sets

generated include helium, thorium, and the constituent elements for concrete. All cross sections were originally generated in the 231-25 coupled neutron-gamma energy group structure and then collapsed to the 51-25 group structure. Below 15 MeV, the 231 neutron group structure is equivalent to the 239 group structure adopted as a standard by Cross-Section Evaluations Working Group (CSEWG). The helium cross sections were generated with a $1/E$ weighting function used for both the 231-25 and 51-25 sets. The concrete constituent elements cross sections were generated in the 231-25 group structure by use of $1/E$ or $1/(E\sigma_T)$ weighting functions and collapsed to 51-25 structure.

Weighting spectra for collapsing 231-25 cross-section sets for concrete constituents into the 51-25 group structure were created by the XSDRN code⁴ from the flux energy distributions through the PCRV at the axial core midplane and below the revised lower axial shield. Additional weighting spectra were created to determine the effects of high iron content (rebar) concrete, the effect of weighting with the spectra at 0.3 m (1 ft) from the PCRV liner, which is approximately the position of the closest tendon, and the effect of using a relatively high CaCO_3 mix similar to the concrete employed for TSF experiments. After examination of the collapsing spectra, concrete cross sections were obtained in the 51-25 group structure for the 0.3-m (1-ft) PCRV weighting function, the rebar concrete spectrum [similar to a 2.7-m (9-ft) PCRV spectrum], and the TSF concrete spectrum. Three 231-25 cross-section sets were prepared for thorium. Two sets were prepared with $1/E$ -to-Maxwellian weighing but with different resonance treatments in the unresolved region: the first was treated as infinitely dilute, the second was given a Bondarenko-type treatment⁵ with $\sigma_0 = 22$. The third thorium set began with a Bondarenko treatment of the unresolved region and added a GAROL-type⁶ resonance overlap (pointwise energy mesh weighting) and an eight-zone ThO_2 blanket rod spatial self-shielding model treatment for the resolved resonance region (4500 to 0.1 eV) using the ROLAIDS code.⁷ A fourth 231-25 thorium cross-section set was generated with the ROLAIDS code for a two-zone spatial self-shielding model. Comparison of the eight-zone ROLAIDS-produced cross sections with the Bondarenko and

infinite dilution sets indicated sizable differences near significant resonance structure. However, little difference was noted between the eight-zone and two-zone ROLAIDS models. The 1/E and Bondarenko 231-25 sets were collapsed to the 51-25 structure by use of the radial blanket spectrum generated in the 50-group calculations, and the ROLAIDS two-zone set was collapsed to 51-25 by use of the point-energy spectrum generated by XLACS⁷ during the formation of the 231-group set.

Although no gamma production data are available for thorium in the ENDF/B files, two sets of ^{232}Th gamma production cross sections were obtained in the 51-25 format. Inelastic gamma production was obtained for both sets by adapting the ^{238}U inelastic gamma production distribution and normalizing it by direct proportion to the ^{232}Th - ^{238}U inelastic cross-section ratios as a function of incident neutron energy group. Capture gamma production was adopted from a Nuclear Data report⁸ for one cross-section set and from a GA report by Engholm⁹ for the other set. The methods used to obtain these two gamma-production cross-section sets are obviously stop-gap, and a real need exists to obtain good measurements of ^{232}Th gamma-production cross sections and complete ENDF/B data files. Although thorium gamma production does not appear to be a determining consideration for radial shield design, it will be crucial for future investigations of heating in the radial blanket and at the core-blanket interface.

4.1.3 Shielding Development Program Plan

The ORNL contribution to the GCFR program plan included the assessment of areas of concern discussed in Sect. 4.1.1, the development of a long-term schedule and cost estimates to satisfy these concerns, and an analysis of the impact of LMFBR-generated shielding methods, calculations, and experiments on the GCFR shielding program. The full program plan⁶ was written and assembled at GA¹⁰ and, after the normal series of reviews and iterations, basically agreed to at a meeting in May. The development of the shielding program plan stands as one of the major accomplishments of FY 1975, and formal adoption is expected early in FY 1976.

4.1.4 Study of the Sensitivity of the Flux (>1 MeV) at the PCRV Liner to Cross Sections in the One-Dimensional Radial Traverse (Core Midplane) Model

This particular sensitivity study (high-energy flux at the PCRV liner for the reference radial shield) is indicative of some of the information that may be obtained from this type of study. The sensitivity study was performed with the one-D discrete ordinates code ANISN¹¹ and the sensitivity analysis code SWANLAKE.¹² The sensitivity by zone is presented in Table 4.1 and shows that the high-energy flux at the

Table 4.1. Cross-Section Sensitivities of High-Energy (>1 MeV) Flux at PCRV Liner

Zone	Significant Type of Material	Sensitivity ^a		P_1 Sensitivity ^b
		Zone	Material Total	
1	Core	-0.0225		
2	Core	-0.0680		
3	Core	-0.241		
4	Core	-0.715	-1.05	+1.5
5	Radial blanket	-4.25	-4.24	-31.0
6	Coolant	-0.0470		
7	Graphite	-1.53		
8	304 SS	-2.34		
9	Coolant	-0.0163		
10	304 SS	-1.72		
11	Graphite	-2.14		
12	304 SS	-1.97		
13	Graphite	-1.62	-5.29	
14	304 SS	-2.49	-8.52	-47.1
15	Coolant	-0.198	-0.26	+0.5
16	Liner	-0.0147	+0.01	+2.35
17	PCRV	+0.0020	0.00	-0.31
SYSTEM TOTAL			-19.4	-84.5

^aNumbers are percent changes in response [$\phi(E > 1$ MeV) at the PCRV liner] per percent increase in cross section, as predicted by linear perturbation theory.

^bNumbers are percent change in response [$\phi(E > 1$ MeV) at the PCRV liner] due to changing from P_3 to P_1 Legendre expansion of the flux, as predicted by linear perturbation theory.

liner is very sensitive to cross sections used in the radial model. The total sensitivity of -19.4 indicates that increasing all cross sections in the system by 1% would decrease the high-energy flux at the liner by 19.4%, according to linear perturbation theory. The highest magnitude of sensitivity for any zone is that for the radial blanket, zone 5, of -4.25. The type 304 stainless steel and graphite zones of the heterogeneous radial shield representation are also significant. The P_3 -to- P_1 predictions are given as the total predicted effect of the mixture over the system, and again indicate the importance of the radial blanket. The P_L predictions also give some indication of the degree of linearity of the system, since the total predicted effect of -84.5% may be compared with the calculated value of 44.2%. This difference is typical of linear restrictions, since a predicted change of 84% is beyond the linear range. A direct study of the effects of the various radial zones is presented in Table 4.2, where the largest reduction in the high-energy flux due to nuclear interaction clearly occurs in the

Table 4.2. Reduction in Flux (>1 MeV) for the GCFR Reference Radial Shield

Zone	$\phi(E > 1 \text{ MeV}) \text{ Coming into Zone} / \phi(E > 1 \text{ MeV}) \text{ Leaving Zone}$		
	Nuclear Interaction	Geometric (r^2)	Total
Radial blanket	50.3	2.2	128.9
Coolant	1.0	1.4	1.4
Inner shield graphite layer	3.4	1.2	4.0
Inner shield SS ₃₀₄ layer	5.6	1.2	6.5
Coolant	1.1	1.1	1.2
Outer shield 1st SS ₃₀₄ layer	3.5	1.1	3.9
Outer shield 1st graphite layer	10.2	1.2	11.9
Outer shield 2nd SS ₃₀₄ layer	5.7	1.1	6.3
Outer shield 2nd graphite layer	5.2	1.1	5.7
Outer shield 3rd SS ₃₀₄ layer	11.6	1.1	13.2
Outlet coolant	1.5	1.3	2.0
TOTAL, radial blanket through outlet coolant	2.3×10^7	10	2.3×10^8

radial blanket, again confirming the sensitivity results. High reductions are also noted for the first graphite layer and the last type 304 stainless steel layer of the outer shield. Cross-section sensitivities by element and zone are presented in Tables 4.3 and 4.4, indicating especially high sensitivity to thorium and iron cross sections. In using the sensitivity analysis technique for more advanced design problems, the sensitivities for partial cross sections (such as inelastic and capture) would be determined and then folded with estimates of the uncertainties in these cross sections to give an estimated uncertainty in the result [here, the flux (>1 MeV) at the PCRV liner] due to cross-section uncertainties.

Table 4.3. Sensitivity of High-Energy (>1 MeV) Flux at PCRV Iron Liner to Elements of Stainless Steel in Core, Blanket, and Shield

Zone	Sensitivity for Each Element				Sensitivity for Type 304 SS
	Iron	Nickel	Chromium	Molybdenum	
1 Core	-6.3×10^{-3}	-1.3×10^{-3}	-1.5×10^{-3}	-2.8×10^{-4}	
2 Core	-1.91×10^{-2}	-3.9×10^{-3}	-4.5×10^{-3}	-8.6×10^{-4}	
3 Core	-0.0676	-0.0139	-0.0163	-3.0×10^{-3}	
4 Core	-0.194	-0.0395	-0.0492	-8.6×10^{-3}	
5 Blanket	-0.873	-0.172	-0.219	-0.0375	
8 Shield	-1.64	-0.22	-0.48		-2.34
10 Shield	-1.21	-0.16	-0.35		-1.72
12 Shield	-1.37	-0.19	-0.41		-1.97
14 Shield	-1.74	-0.23	-0.52		-2.49
Core TOTAL	-0.26	-0.06	-0.07	-0.05	
Blanket TOTAL	-0.87	-0.17	-0.22		
Shield TOTAL	-5.96	-0.80	-1.76		-8.52
P_1 Sensitivity					
Core	+0.4	+0.1	0.0	-0.2	
Blanket	-7.0	-1.9	-0.2		
Shield	-32.3	-4.6	-10.2		-47.1

The results of this sensitivity analysis indicate the importance of calculating through ThO_2 and type 304 stainless steel and, since iron cross sections have been fairly well investigated, especially indicate the importance of "good" (low-uncertainty) cross sections for thorium, and also for chromium and nickel.

Table 4.4. Sensitivity of High-Energy (>1 MeV) Flux at PCRV Iron Liner to Elements of Thorium Oxide in Radial Blanket (Zone 5)

Element	Sensitivity	
	Cross Section	F_1
^{232}Th	-2.03	-17.1
0	-0.88	-3.2
ThO_2	-2.91	-27.3

4.1.5 Study of Cross-Section, Calculational Parameter, and Modeling Effects on Calculated Results for the Initial Radial Shield Configuration

A detailed evaluation of the effects of cross sections and calculational parameters on calculations of the one-dimensional radial reference design neutron flux was completed, including comparative one- and two-dimensional results. The results are summarized in Table 3.5, where the flux levels given are per source neutron. To note some of the more significant comparisons, the 2-D calculated flux levels are a factor of 2 higher at the PCRV liner for both high- and low-energy neutrons. The new 51-group cross sections increased the high-energy flux slightly ($\sim 20\%$) but halved the thermal flux at the liner. However, the thermal flux transmitted through the radial blanket was increased by $\sim 40\%$ by the ROLAIDS resolved resonance treatment for the thorium cross section (the 50-group sets were infinitely dilute $1/E$ weighted). Going from P_1 to P_3 Legendre expansions of the angular dependence of the flux doubled the high-energy flux at the liner. In the final row, the inner and outer radial shields were represented by separate graphite and type 304 stainless steel layers instead of by a homogeneous 50-50 graphite and type 304 stainless steel mixture. The heterogeneous shield representation resulted in a 30% increase in the high-energy flux transmitted to the liner, while the thermal flux was increased in the outer portion of the radial blanket by approximately a factor of 5 because of reflection from the graphite first layer of the inner shield, but it is 60% lower at the liner.

Table 4.5. GCFR Fast and Thermal Flux Calculation Comparisons for the Reference Radial Shield Design at Core Midplane

Calculation	Normalized Flux at Various Locations			
	Outer Boundary of			PCRV Liner
	Core	Radial Blanket	Inner Shield	
<u>Fast Flux (>1 MeV)</u>				
DOT 2-dimension				
P ₁ 50 group homogeneous	1.7×10^{-5}	1.2×10^{-7}	2.5×10^{-9}	1.3×10^{-14}
ANISN 1-dimension				
P ₁ 50 group homogeneous	4.7×10^{-6}	2.8×10^{-8}	4.8×10^{-10}	5.1×10^{-15}
P ₁ 51 group homogeneous	3.9×10^{-6}	2.9×10^{-8}	5.2×10^{-10}	6.4×10^{-15}
P ₃ 51 group homogeneous	3.9×10^{-6}	3.0×10^{-8}	6.3×10^{-10}	1.3×10^{-14}
P ₃ 51 group heterogeneous	3.9×10^{-6}	3.0×10^{-8}	8.7×10^{-10}	1.7×10^{-14}
<u>Thermal Flux (<2.38 eV)</u>				
DOT 2-dimension				
P ₁ 50 group homogeneous	1.8×10^{-10}	1.6×10^{-8}	2.6×10^{-9}	6.3×10^{-12}
ANISN 1-dimension				
P ₁ 50 group homogeneous	1.2×10^{-10}	1.8×10^{-8}	2.5×10^{-9}	3.4×10^{-12}
P ₁ 51 group homogeneous	1.2×10^{-10}	2.5×10^{-8}	2.1×10^{-9}	1.8×10^{-12}
P ₃ 51 group homogeneous	1.2×10^{-10}	2.5×10^{-8}	2.1×10^{-9}	2.1×10^{-12}
P ₃ 51 group heterogeneous	1.7×10^{-10}	1.3×10^{-7}	6.1×10^{-9}	1.3×10^{-12}

Calculational results for neutron and gamma-ray heating are compared similarly in Table 4.6. The units used correspond to those given for allowable heating rates.¹ The heating at the first tendon position in the PCRV is given in rads absorbed in tissue and rads absorbed in concrete since a set of kerma factors for the tendon lubricant was not yet available. For all cases, the neutron heating is not significant compared with the gamma heating at the same position. Increasing the Legendre expansion from P_1 to P_3 increased the heating by up to 50%, but using a heterogeneous radial shield representation reduced the heating to levels similar to or lower than those calculated in the P_1 homogeneous case. The tendency of heterogeneous radial shield representation to lower the gamma heating is consistent with the previously noted increase in the high-energy flux at the liner, thereby decreasing the low-energy flux transmitted through the shield and the number of gamma rays produced by capture of low-energy neutrons in iron.

Table 4.6. GCFR Heating Calculational Comparisons for the Reference Radial Shield Design

Position	Value by Each Calculation		
	P_1 51-25 Homogeneous	P_3 51-25 Homogeneous	P_3 51-25 Heterogeneous
<u>Neutron Heating</u>			
PCRVR inside liner, W/m ³	6.1×10^{-22}	8.8×10^{-22}	1.9×10^{-21}
PCRVR at first tendon, rads-concrete	8.2×10^{-25}	1.2×10^{-24}	1.5×10^{-24}
<u>Gamma Heating</u>			
PCRVR inside liner, W/m ³	8.7×10^{-20}	1.1×10^{-19}	6.7×10^{-20}
PCRVR at first tendon, rads-concrete	5.7×10^{-22}	8.2×10^{-22}	5.9×10^{-22}
rads-tissue	6.4×10^{-22}	9.7×10^{-22}	6.7×10^{-22}

The calculated flux and heating levels are compared with currently recommended maximum levels in Table 4.7. The results show that, according to these 1-D calculations, the preliminary model of the GCFR radial shield meets all given constraints. The tendon lubricant heating should be reevaluated when kerma factors are available for the grease. The homogeneous radial shield calculations referred to are the results obtained from the high-angular-quadrature lower axial 2-D calculation made with the original 50-group cross-section set. The heterogeneous figures are the result of a series of 2-D calculations that were run on the heterogeneous reference model by use of a P_3 Legendre expansion of the 51-25 cross-section set. A core k_{eff} calculation, which includes the radial and axial blankets and adjacent upper and lower axial regions,

Table 4.7. A Comparison of Currently Recommended Maximum Radiation Levels with One- and Two-Dimensional Calculated Values for Preliminary Models of the GCFR Radial Shield Along the Core Midplane

Parameter and Position	Recommended Constraint ^a	Calculational Results ^b	
		2-D, P_1 , 50 Group	1-D, P_3 , 51-25 Group Heterogeneous Radial Shield
Flux (>1 MeV) PCRV liner, $\text{n cm}^{-2} \text{ sec}^{-1}$	2×10^9	9.2×10^5	1.2×10^6
Flux (<2.38 eV) PCRV liner, $\text{n cm}^{-2} \text{ sec}^{-1}$	1×10^9	4.5×10^1	9.2×10^7
Heating in PCRV, W/m^2	1000		4.8
Heating in tendon lubricant (rads in grease)	0.1 ^c		0.042 ^d

^aConstraint originally stated as $2 \times 10^{18} \text{ n/cm}^2$ over plant lifetime.

^bFlux levels normalized to a total flux of $7.1 \times 10^{19} \text{ n cm}^{-2} \text{ sec}^{-1}$.

^cConstraint originally stated as 1×10^9 rads over plant lifetime (40 years at 80% operational).

^dRads in concrete.

gave a k_{eff} value of 1.07, which is in reasonable agreement with the GA value of 1.06. The second calculation included the full 2-D model up through the grid plate. The core calculation served as a source for the second run, which was calculated with an S_6 quadrature (30 angles). The angular flux was taken from this 2-D calculation at a vertical position just below the assembly bottom fitting, and then transported

below the outer shield and up the outlet coolant duct by use of a biased 302 angular quadrature. Table 4.8 presents the results of this calculation at some positions of interest. The lower axial helium channel is located below the lower stainless steel pre-shield. The wrap-around region is the vertical level under the outer shield where an additional shield section has been appendaged to the liner. The streaming path designation refers to the position at which particles streaming under the outer shield and up the outlet coolant channel meet the PCRV liner.

Table 4.8. Comparison of Currently Recommended Maximum Radiation Levels with Two-Dimensional Calculated Values for the Homogeneous and Heterogeneous Reference Models of the Lower Axial Area of the GCFR Outer Radial Shield

Location	Shield Model	Flux ^a at PCRV Liner, $n\text{ cm}^{-2}\text{ sec}^{-1}$		Heating in PCRV (W/m^2)	Heating in Tendon Lubricant (rads in grease)
		>1 MeV	<2.38 eV		
Recommended constraint		2×10^{10} ^b	1×10^9	1000	0.1 ^c
Lower axial He channel	Homogeneous	5×10^7	3.3×10^{10}	700	5.5 ^d
	Heterogeneous	2.4×10^7	3.7×10^{10}		
Wrap-around region	Homogeneous	8×10^7	6.1×10^{10}	1000	7.7
	Heterogeneous	7.1×10^7	3.3×10^{10}		
Streaming path	Homogeneous	6×10^7	7.8×10^{10}	1800	12
	Heterogeneous	5.3×10^7	7.8×10^{10}		

^a Flux levels normalized to a total flux of $7.1 \times 10^{11}\text{ n cm}^{-2}\text{ sec}^{-1}$.

^b Constraint originally stated as $2 \times 10^{10}\text{ n/cm}^2$ over plant lifetime.

^c Constraint originally stated as 1×10^8 rads over plant lifetime (40 years at 80% operational).

^d Rads in concrete.

The high-energy flux peaks in the wrap-around region for both calculations, but is still well below the established criteria. The thermal flux peaks at the streaming path position and does exceed the current criterion but could be reduced by boronating the graphite in the reference outer shield. The results indicate no significant difference for the peak values of the high-energy flux, but do show a factor of 2 difference in the thermal flux in the wrap-around region, presumably due to the heterogeneity effect noted in Table 4.5. Calculated gamma heating and dose rates in the PCRV

are also given for the heterogeneous case. The heating in the PCRV peaks in the streaming path and is marginal, but should also be affected by boronation of the outer shield graphite. Damage to the tendon lubricant also peaks in the streaming path. As mentioned earlier, this response is not definitive, but does indicate an area for concern, although this response should also be reduced by boronation of outer shield graphite. While none of the parameters investigated peak in the region of the lower axial helium channel, this region is also of some concern. A revised shield configuration could have the outer shield graphite boronated to mitigate the effects of reducing the outer shield and moving in the PCRV liner by 0.3 m (1 ft). However, the effect of the boronation on the parameters at the lower helium gap is not yet clear.

4.1.6 Comparative Calculations of Reference and Revised Models for the Outer Radial Shield

The general conclusion reached on the basis of the calculations described in Sect. 4.1.5 was that the reference radial shield design was basically conservative. The economic incentive to reduce the radius of the reactor cavity therefore led to an effort by GA to redesign the outer radial shield¹³ to reduce the radius of the reactor cavity by about 0.3 m (1 ft). This section compares calculated results for selected redesign options with calculations made with the reference shield configuration.

A series of calculations of flux and heating rates along the core midplane radial axis was completed, and the comparative results are presented in Table 4.9. All calculations used the 51-25 cross-section set with a P_3 Legendre expansion and a heterogeneous representation of the inner and outer radial shields. The normal reference shield configuration is the one that has been in use for previous calculations. The alternate reference shield configuration is a different r - θ path through the reference radial shield in which the interior stainless steel region of the outer radial shield is replaced by a graphite region. The revised shield configuration has the same inner shield as the reference model, but the outer shield thickness has been reduced by 0.3 m (1 ft), and the graphite region of the outer radial shield has been boronated. The

Table 4.9. A Comparison of Recommended Maximum Radiation Levels with One- and Two-Dimensional Calculated Values for Various Preliminary Models of the GCFR Radial Shield Along the Core Midplane

Calculation	Shield Model	Flux ^a at PCRV Liner, $n\text{ cm}^{-2}\text{ sec}^{-1}$		Heating in PCRV (W/m^2)	Heating in Tendon Lubricant (rads in grease)
		>1 MeV	<2.38 eV		
Recommended constraint		$2 \times 10^{10}\text{b}$	1×10^9	1000	3.1 ^c
2-D	Reference	5.6×10^5	3.0×10^9	7.2	0.10
1-D	Reference	1.2×10^6	9.2×10^7	4.8	0.042 ^d
1-D	Alternate Ref.	2.3×10^6	8.1×10^7	1460	0.25
1-D	Revised	7.8×10^7	1.1×10^9	150	1.6
1-D	Revised, No B ₄ C	8.5×10^7	$5. \times 10^6$	330	2.8

^aFlux levels normalized to a total flux of $7.1 \times 10^{19}\text{ n cm}^{-2}\text{ sec}^{-1}$.

^bConstraint originally stated as $2 \times 10^{10}\text{ n/cm}^2$ over plant lifetime.

^cConstraint originally stated as 1×10^8 rads over plant lifetime (40 years at 83% operational).

^dRads in concrete.

DOT 2-D calculation was performed on the reference radial shield configuration. The interpretation of these results should be conditioned by the fact that the radial shield design is currently undergoing revision, so that these results are primarily useful as input information for development of new radial shield designs. The high-energy flux constraint is met by every shield configuration considered. The thermal flux criterion is met by all designs except for the revised design without B₄C. The PCRV heating constraint is in question only for the alternate $r-\theta$ path for the reference design. The results for the damage to the tendon lubricant are not definitive since the response function used does not represent the dose to the specific grease employed, but they certainly do indicate an area for future investigation, especially for a revised radial shield design.

On the basis of these one-dimensional calculations, a series of calculations of the flux and heating rates was completed for a new revised model in which the thorium oxide density in the radial blanket was increased by 11% and in which the radial cavity thickness was decreased by approximately 0.3 m (1 ft) by a reduction in the outer radial shield. The revised outer radial shield used for these

calculations consisted of approximately 0.18 m of graphite followed by 0.18 m of boronated graphite (19% B₂C by weight) and 50 mm of type 304 stainless steel. Both one- and two-dimensional calculations were performed by use of the P₃ 51-25 coupled cross section set, and the results obtained are compared with those obtained from similar calculations for the initial or reference model in Table 4.10. As part of this overall calculational effort a helium-worth calculation was made for the core

Table 4.10. Comparison of Currently Recommended Maximum Radiation Levels with Calculated Values for the Initial and a Revised^a Model of the GCFR Radial Shield

Calculation	Shield Model	Plane of Measurement	Flux ^b at PCRV Liner, n cm ⁻² sec ⁻¹		Heating in PCRV (W/m ²)	Heating in Tendon Lubricant (rads in grease)
			>1 MeV	<2.38 eV		
Recommended constraint			2 × 10 ¹³ ^c	1 × 10 ⁹	1000	0.1 ^d
1-D	Initial	Core midplane	1.2 × 10 ⁶	9.2 × 10 ⁷	4.8	0.042 ^e
2-D	Initial	Core midplane	5.6 × 10 ⁵	3.0 × 10 ⁸	7.2	0.10
1-D	Revised	Core midplane	1.6 × 10 ⁸	3.5 × 10 ⁸	350	2.4
2-D	Revised	Core midplane	1.2 × 10 ⁸	3.9 × 10 ⁸	200	1.6
2-D	Initial	Lower axial He channel	2.4 × 10 ⁷	3.7 × 10 ¹⁰	700	5.5 ^e
2-D	Revised		9.9 × 10 ⁷	4.6 × 10 ¹⁰	900	7.1
2-D	Initial	Wrap-around region	7.1 × 10 ⁷	3.3 × 10 ¹⁰	1000	7.7
2-D	Revised		1.8 × 10 ⁸	8.5 × 10 ⁹	300	4.3
2-D	Initial	Maximum above ^f wrap-around region	5.3 × 10 ⁷	7.8 × 10 ¹⁰	1800	0.3
2-D	Revised		5.0 × 10 ⁸	1.6 × 10 ⁹	300	2.4

^aRevised model has about 0.3 m (1 ft) thinner outer shield (and correspondingly reduced reactor cavity radius) consisting of about 0.18 m graphite followed by about 0.18 m boronated graphite (19 wt % B₂C) and by about 50 mm type 304 stainless steel.

^bFlux levels normalized to a total flux of 7.1×10^{19} n cm⁻² sec⁻¹.

^cConstraint originally stated as 2×10^{18} n/cm² over plant lifetime.

^dConstraint originally stated as 1×10^6 rads over plant lifetime (40 years at 80% operational).

^eRads in concrete.

^fFor the initial design this maximum occurs where the lower axial streaming path meets the PCRV liner. For the revised design it occurs at an axial position just below the inner shield.

with a revised radial blanket density. The k effective with helium present was 1.068. The k effective without helium present was 1.071, giving a helium-worth of 70 cents. In interpreting the table shown it should be noted that the constraints applied were adapted from the HTGR program and that constraints specifically applicable to the GCFR are gradually evolving. The constraint levels stated in this table are those that have been used at ORNL during the past fiscal year, and should then be viewed as current indications of desired levels rather than as fixed maximum allowable radiation levels. Table 4.10 compares 1- and 2-D calculations for the initial and revised radial shield designs along the core midplane. The revised design was successful in meeting the current constraints except for the dose to the tendon lubricant. As indicated previously, calculations for this constraint are not yet felt to be definitive because of the differences in the response functions (rads in silicon versus rads in tissue) and questions regarding the exact position of the tendon in the PCR. Table 4.10 also compares 2-D results at positions of special interest in the lower axial region for the initial and revised radial shield design. The results indicate that the revised design was reasonably successful in meeting current design criteria, especially those for high-energy flux levels and for heating in the PCR. The thermal flux level criterion itself is currently in review, with a new criterion of 1×10^{10} quite likely. This would render the thermal flux levels fairly acceptable with the exception of the lower axial helium channel position, where the radial shield remains an area for further investigation, with a new constraint (0.5 versus 0.1 rads/sec) also quite likely. In general, results for the revised design are encouraging in regard to reducing the reactor cavity radius.

A series of adjoint calculations for the two-dimensional revised design were initiated. These will include calculations for the thermal flux levels in the PCR liner at the lower axial helium channel and wrap-around positions, PCR gamma heating at the lower axial helium channel, and gamma dose in the tendon lubricant at the lower axial helium channel and wrap-around positions. These calculations will be applied in the

cross section sensitivity studies to determine neutron transport mechanisms and gamma production and transport mechanisms in the current revised design and to indicate the effects of potential design changes on these responses.

4.2 DESIGN AND PERFORMANCE OF THE BASIC ROD STREAMING EXPERIMENTS - C. E. Clifford

The basic rod streaming experimental configurations were designed, and experimental measurements were planned and scheduled. Measurements will be taken in single-cell and multiple-rod-array cylindrical geometries with about 50% rod fraction by volume. The rods to be used in the experiments are about 1.1 m (0.45 in.) in diameter and 1.37 m (54 in.) in overall length, with 76 mm (3 in.) of aluminum at each end and an intermediate 1.22 m (48 in.) of 1.3%-enriched UO_2 .

The single-cell basic rod streaming experimental configurations have been fabricated and assembled. Current plans call for measurements of the flux emerging from cells in which collimated and partially collimated beam sources are incident on void cells and cells containing an iron rod and an aluminum-clad UO_2 rod. The grid plates for use in the full-array experimental configuration have been fabricated. They will support 859 rods in a triangular-pitch array that is approximately 50% void. The plates will fit inside a 0.459-m-diam by 1.416-m-long (18 1/16- by 55 3/4-in.) pipe. Both the single-cell and the full-array configurations are enclosed in water-filled aluminum tanks.

A thin streaming experiment utilizes the new broad beam source from the Tower Shielding Reactor, which is estimated to have 200 times the intensity of the small beam. A spectral modifier has been incorporated into the experimental configuration to give a source spectrum more typical of the leakage spectrum from the GCFR core. Measurements have been completed on the neutron transport through the 0.10-m (4-in.) steel and 25-mm (1-in.) boral spectrum modifier utilizing the 0.076-, 0.15-, and 0.25-m (3-, 6-, and 10-in.) Bonner balls.* The water tank

*Bonner balls are neutron detectors embedded in polyethylene spheres. The size of the sphere governs the spectral response of the detector.

for the single-cell experiment was delivered and placed in position. Preamalysis of the experiment showed that the configuration needed to be modified to improve the small collimator geometry. Measurements were taken of the foreground and background with the hole empty and also with the iron rod in place in the hole. The direct beam with no rod in the hole was only a 10 times background. With the iron in position, the readings from the hole were well below background. These unfavorable conditions resulted in a decision to proceed to the large collimator configuration of the single-cell experiment.

During the month of June, work was completed on the single-cell pin streaming experiment. These measurements were limited to the large collimator because of the relatively high level of background that was observed with this experimental configuration. Measurements were taken with the 0.076-, 0.15-, and 0.25-m (3-, 6-, and 10-in.) Bonner balls for the case with no central rod in the single-cell configuration, the case with the steel rod present, the case with the uranium rod present, and the case with the entire configuration filled with water. Most of these measurements were repeated five or more times, and the counting rate varied from 10 to 15% depending on the exact orientation of the central rods. The 0.46-m-diam (18-in.) full-array configuration (containing 859 rods) is being installed at the present time. It is anticipated that measurements on the full array configuration will be completed during July.

4.3 PREANALYSIS OF THE GCPR STREAMING EXPERIMENT - C. O. Slater

An initial calculation of an iron-rod "cell," consisting of an iron rod surrounded by air, was made with the DOT two-dimensional discrete ordinates transport code² to examine the effects of streaming in a simulated GCPR fuel-element geometry. The 0.76-m-long (30-in.) cell consists of a 7.2-mm-diam (0.285-in.) iron rod with a surrounding 2.57 mm (0.101-in.) air-streaming gap, which simulates the triangular pitch between fuel rods with a fuel element. The results of this calculation were to be compared with those for a homogeneous iron-air cell of similar dimensions as an initial step in designing a basic rod streaming experiment.

Calculations for the preliminary version of the basic rod streaming experiment continued. A comparison of heterogeneous vs homogeneous DOT (2-D) calculations of an iron-rod-and-air "cell" indicated streaming that may be sufficient for experimental purposes. A geometrical model, similar to a GCFR fuel element, composed of an iron-rod lattice in air was set up for MORSE¹⁴ Monte Carlo calculations to compare with the results of the DOT cell case.

Additional calculations were initiated to determine the effects of varying the size and composition of the experimental configuration and the angular distribution of the experimental source. Using a 0.19-m (7.5-in.) unreflected radius for a homogeneous iron and air configuration gave one sixth the transmitted flux obtained from a reflected case (equivalent to a one-dimensional infinite source calculation). Increasing the unreflected radius to 0.38 m (15 in.) gave one half the results of the reflected case. A one-dimensional ANISN calculation of a depleted UO₂-air experimental configuration gave one fourth the transmitted flux for the iron-air case. This was an indication that streaming through a configuration of UO₂ rods should be more significant than streaming through an iron rod configuration. Varying the angular distribution of the source shows that the most forward-peaked portion of the source contributes 16% of the transmitted flux although comprising only 2% of the total source neutrons.

Results obtained up to this point indicated potentially large effects for forward-peaked source particles and for UO₂ rods. The need for a heterogeneous versus homogeneous experimental comparison, presumably for iron rods parallel to the beam compared with iron plates perpendicular to the beam was also quite evident.

Analysis began on possible single-cell experimental configurations for a partially and a severely collimated beam source incident on void, iron rod, and aluminum-clad UO₂ rod cells. The adequacy of techniques available to calculate uncollided flux emerging from the single-cell configuration was investigated. An analytic solution was obtained for the problem and used as a standard for comparison. A MORSE Monte Carlo calculation came within 1% of the uncollided flux emerging from the cell

from a source within a rod, but DOT discrete-ordinates calculations, even with highly biased quadrature sets, were not satisfactory. However, when the GRTUNCL (known as the "Great Uncle") code,¹⁵ which was written for use with DOT to give a first collision source, was used in the adjoint mode, agreement was excellent. It should be noted that this calculation represents a severe test, since all the flux lies within an angular cosine of 0.999 to 1.0, and most is within 0.999999 to 1.0. When calculations were made on a void uncollimated cell, DOT and MORSE showed reasonable agreement. Calculations were then begun to determine the flux emerging from the face of the cell configurations, collimated and uncollimated, with rods present. The emerging flux was to be calculated for specific angular orientations, and compared with the flux emerging from the water-filled tanks surrounding the cell. The results of these calculations and comparisons is presented on the following self-contained report.

4.3.1 Preliminary of the Single-Cell Streaming Experiments

The preliminary of the GCFR single-cell streaming experiments was carried out with DOT/FALSTP¹⁵ and checked in several instances with MORSE. The specifications for the experiments are given in Sect. 4.2 and are not detailed here. However, the experimental setup is shown in Fig. 4.3.

The calculations were carried out with a ten-group energy structure and were intended to show qualitatively what one should expect in the way of experimental measurements. They should give an indication of which experiments can be carried out with little difficulty and which ones may be carried out with a slight modification of the experimental setup.

Seven cases were examined: (1) an empty cell without source collimation, (2) an empty cell with source collimation, (3) an iron pin in the cell without collimation, (4) an iron pin in the cell with collimation, (5) a UO₂ pin in the cell without collimation, (6) a UO₂ pin in the cell with collimation, and (7) the tank and all channels filled with water. The latter case when compared with the other cases gives the excess transmission provided by each of the other cases.

Fig. 4.3. Experimental Setup for the GCFR Single-Cell Streaming Experiment. To convert dimensions to millimeters, multiply by 25.4.

4.3.1.1 Results

Table 4.11 gives the relative leakages from the system subdivided into the tank and cell leakages. In addition, the cell leakage is subdivided into the annulus and pin leakages. For cases 1, 2, and 7 the pin and annulus leakages are the leakages over the regions defined by the iron or UO_2 pin. Table 4.11 also gives the ratio of the cell leakage to the tank leakage for three energy ranges as well as for all energies. It is noted that with sufficient poisoning of the water in the tank, the leakage above 122.8 keV dominates for cases 1, 2, 3, and 5. For these cases, the leakage above 122.8 keV from the cell is at least 8 times that from the tank. The leakages for cases 3 and 5 seem to be attributable to direct streaming from the source through the annular gap about the pin, since the leakages are about the same in both cases. The fissions in the UO_2 did not appreciably increase the cell leakage above that of the iron pin in case 3. A greater effect

Table 4.11. Neutron Leakages from the Experimental Assembly

Energy Range	Leakage, $\text{cm}^{-2}\text{sec}^{-1}$					Ratio Cell/Tank
	System	Tank	Annulus	Pin	Cell Total	
<u>Case 1 - Void Cell; Uncollimated Source</u>						
122.0 keV	$2.053 \cdot 10^{-7}$	$1.131 \cdot 10^{-7}$	$8.772 \cdot 10^{-7}$	$1.182 \cdot 10^{-7}$	$2.040 \cdot 10^{-7}$	153
2.382 eV-122.0 keV	$2.659 \cdot 10^{-7}$	$1.684 \cdot 10^{-7}$	$9.073 \cdot 10^{-7}$	$1.703 \cdot 10^{-7}$	$2.511 \cdot 10^{-7}$	16.9
2.382 eV	$6.805 \cdot 10^{-7}$	$5.998 \cdot 10^{-7}$	$1.168 \cdot 10^{-6}$	$7.601 \cdot 10^{-7}$	$8.769 \cdot 10^{-7}$	0.146
entire	$4.144 \cdot 10^{-7}$	$6.017 \cdot 10^{-7}$	$1.055 \cdot 10^{-6}$	$2.113 \cdot 10^{-7}$	$7.167 \cdot 10^{-7}$	0.526
<u>Case 2 - Void Cell; Tightly Collimated Source</u>						
122.0 keV	$2.328 \cdot 10^{-7}$	$2.032 \cdot 10^{-7}$	$4.535 \cdot 10^{-7}$	$1.172 \cdot 10^{-7}$	$1.826 \cdot 10^{-7}$	8.99
2.382 eV-122.0 keV	$3.354 \cdot 10^{-7}$	$1.900 \cdot 10^{-7}$	$9.979 \cdot 10^{-7}$	$1.371 \cdot 10^{-7}$	$2.349 \cdot 10^{-7}$	2.36
2.382 eV	$1.643 \cdot 10^{-7}$	$1.441 \cdot 10^{-7}$	$2.436 \cdot 10^{-7}$	$2.613 \cdot 10^{-7}$	$5.019 \cdot 10^{-7}$	0.035
entire	$2.197 \cdot 10^{-7}$	$1.471 \cdot 10^{-7}$	$3.154 \cdot 10^{-7}$	$3.923 \cdot 10^{-7}$	$7.081 \cdot 10^{-7}$	0.481
<u>Case 3 - Iron Pin in Cell; Uncollimated Source</u>						
122.0 keV	$7.279 \cdot 10^{-7}$	$6.845 \cdot 10^{-7}$	$6.438 \cdot 10^{-7}$	$1.562 \cdot 10^{-6}$	$6.594 \cdot 10^{-7}$	9.63
2.382 eV-122.0 keV	$9.350 \cdot 10^{-7}$	$3.377 \cdot 10^{-7}$	$5.746 \cdot 10^{-7}$	$2.329 \cdot 10^{-6}$	$5.979 \cdot 10^{-7}$	1.77
2.382 eV	$1.034 \cdot 10^{-6}$	$1.028 \cdot 10^{-6}$	$5.743 \cdot 10^{-7}$	$5.166 \cdot 10^{-6}$	$6.260 \cdot 10^{-7}$	0.006
entire	$1.116 \cdot 10^{-6}$	$1.038 \cdot 10^{-6}$	$7.507 \cdot 10^{-7}$	$2.298 \cdot 10^{-6}$	$7.817 \cdot 10^{-7}$	0.075
<u>Case 4 - Iron Pin in Cell; Tightly Collimated Source</u>						
122.0 keV	$4.990 \cdot 10^{-7}$	$8.706 \cdot 10^{-7}$	$4.027 \cdot 10^{-7}$	$1.000 \cdot 10^{-6}$	$4.128 \cdot 10^{-7}$	8.76
2.382 eV-122.0 keV	$2.305 \cdot 10^{-6}$	$6.505 \cdot 10^{-7}$	$1.711 \cdot 10^{-6}$	$2.369 \cdot 10^{-6}$	$1.735 \cdot 10^{-6}$	2.67
2.382 eV	$8.505 \cdot 10^{-6}$	$8.133 \cdot 10^{-6}$	$1.675 \cdot 10^{-5}$	$4.267 \cdot 10^{-5}$	$1.717 \cdot 10^{-5}$	0.021
entire	$8.512 \cdot 10^{-6}$	$9.335 \cdot 10^{-6}$	$1.732 \cdot 10^{-5}$	$4.309 \cdot 10^{-5}$	$1.776 \cdot 10^{-5}$	0.021
<u>Case 5 - UO₂ Pin in Cell; Uncollimated Source</u>						
122.0 keV	$7.568 \cdot 10^{-7}$	$8.283 \cdot 10^{-7}$	$6.519 \cdot 10^{-7}$	$2.294 \cdot 10^{-6}$	$6.739 \cdot 10^{-7}$	8.14
2.382 eV-122.0 keV	$1.634 \cdot 10^{-6}$	$6.746 \cdot 10^{-7}$	$6.844 \cdot 10^{-7}$	$7.498 \cdot 10^{-6}$	$7.593 \cdot 10^{-7}$	1.13
2.382 eV	$2.313 \cdot 10^{-6}$	$2.305 \cdot 10^{-6}$	$6.722 \cdot 10^{-6}$	$1.968 \cdot 10^{-5}$	$8.670 \cdot 10^{-6}$	0.004
entire	$2.403 \cdot 10^{-6}$	$2.320 \cdot 10^{-6}$	$7.878 \cdot 10^{-6}$	$4.895 \cdot 10^{-5}$	$8.365 \cdot 10^{-6}$	0.036
<u>Case 6 - UO₂ Pin in Cell; Tightly Collimated Source</u>						
122.0 keV	$8.460 \cdot 10^{-7}$	$2.992 \cdot 10^{-6}$	$5.191 \cdot 10^{-7}$	$2.774 \cdot 10^{-6}$	$5.468 \cdot 10^{-7}$	1.83
2.382 eV-122.0 keV	$1.125 \cdot 10^{-6}$	$4.839 \cdot 10^{-7}$	$5.877 \cdot 10^{-7}$	$5.387 \cdot 10^{-6}$	$6.415 \cdot 10^{-7}$	1.33
2.382 eV	$2.502 \cdot 10^{-6}$	$2.191 \cdot 10^{-6}$	$1.925 \cdot 10^{-6}$	$1.179 \cdot 10^{-5}$	$1.104 \cdot 10^{-5}$	0.162
entire	$2.521 \cdot 10^{-6}$	$2.199 \cdot 10^{-6}$	$2.076 \cdot 10^{-6}$	$1.187 \cdot 10^{-5}$	$1.223 \cdot 10^{-5}$	0.167
<u>Case 7 - Cell and Tank Completely Filled with B₂O</u>						
122.0 keV	$6.205 \cdot 10^{-7}$	$4.792 \cdot 10^{-7}$	$1.278 \cdot 10^{-6}$	$1.495 \cdot 10^{-6}$	$2.683 \cdot 10^{-7}$	0.0004
2.382 eV-122.0 keV	$5.232 \cdot 10^{-7}$	$5.229 \cdot 10^{-7}$	$1.231 \cdot 10^{-6}$	$1.379 \cdot 10^{-6}$	$2.740 \cdot 10^{-7}$	0.0005
2.382 eV	$7.145 \cdot 10^{-6}$	$7.142 \cdot 10^{-6}$	$1.779 \cdot 10^{-5}$	$1.781 \cdot 10^{-5}$	$3.560 \cdot 10^{-5}$	0.0005
entire	$7.144 \cdot 10^{-6}$	$7.143 \cdot 10^{-6}$	$1.780 \cdot 10^{-5}$	$1.780 \cdot 10^{-5}$	$3.560 \cdot 10^{-5}$	0.0005

^aCurrent leaking in n/sec divided by incident flux in $\text{n cm}^{-2}\text{sec}^{-1}$.

of the fissions is shown in the collimated source cases 4 and 6. There the leakage from the UO_2 case almost doubles that for the iron case. The UO_2 total cross section is generally greater than the iron total cross section; hence, the larger leakage for the UO_2 case is attributed to fissions rather than direct transmission through the rod.

Table 4.12 gives relative fluxes at various detector positions beyond the experimental assembly. Table 4.13 gives the $E > 2.38$ eV flux for the same locations. The detector positions are given by the parameters r and θ as shown in Fig. 4.4. These values were computed with FALSTF using the scattering sources generated by DOT. Values at selected locations computed with MORSE are given in Table 4.14.

Table 4.12. Relative Neutron Flux at Selected Positions
Beyond the Experimental Assembly - Calculated
by DOT/FALSTF

Position ^a		Relative Flux			
r (cm)	θ (deg.)	Case 4	Case 5	Case 6	Case 7
76.2	0	$2.293 \cdot 10^{-11}$	$1.173 \cdot 10^{-11}$	$1.584 \cdot 10^{-11}$	$2.793 \cdot 10^{-11}$
76.2	0.68	$6.337 \cdot 10^{-12}$	$3.334 \cdot 10^{-12}$	$2.200 \cdot 10^{-12}$	$2.811 \cdot 10^{-11}$
76.2	10	$1.204 \cdot 10^{-11}$	$1.023 \cdot 10^{-11}$	$1.065 \cdot 10^{-11}$	$2.757 \cdot 10^{-11}$
76.2	30	$2.840 \cdot 10^{-12}$	$9.036 \cdot 10^{-13}$	$9.648 \cdot 10^{-13}$	$2.449 \cdot 10^{-11}$
152.4	0	$1.644 \cdot 10^{-11}$	$2.065 \cdot 10^{-12}$	$1.072 \cdot 10^{-12}$	$8.625 \cdot 10^{-12}$
152.4	0.36	$4.341 \cdot 10^{-12}$	$2.689 \cdot 10^{-12}$	$4.779 \cdot 10^{-13}$	$8.706 \cdot 10^{-12}$
152.4	5	$9.912 \cdot 10^{-12}$	$3.166 \cdot 10^{-12}$	$2.444 \cdot 10^{-12}$	$8.569 \cdot 10^{-12}$
152.4	15	$9.441 \cdot 10^{-12}$	$3.012 \cdot 10^{-12}$	$3.278 \cdot 10^{-12}$	$8.163 \cdot 10^{-12}$
228.6	0	$7.437 \cdot 10^{-12}$	$6.510 \cdot 10^{-13}$	$3.416 \cdot 10^{-13}$	$4.034 \cdot 10^{-12}$
228.6	0.23	$2.659 \cdot 10^{-12}$	$1.862 \cdot 10^{-12}$	$2.924 \cdot 10^{-13}$	$4.082 \cdot 10^{-12}$
228.6	4	$4.635 \cdot 10^{-12}$	$1.483 \cdot 10^{-12}$	$1.630 \cdot 10^{-12}$	$4.011 \cdot 10^{-12}$
228.6	10	$4.530 \cdot 10^{-12}$	$1.448 \cdot 10^{-12}$	$1.589 \cdot 10^{-12}$	$3.920 \cdot 10^{-12}$
243.84	0	$9.285 \cdot 10^{-12}$	$7.419 \cdot 10^{-13}$	$4.004 \cdot 10^{-13}$	$3.565 \cdot 10^{-12}$
243.84	0.21	$2.446 \cdot 10^{-12}$	$1.723 \cdot 10^{-12}$	$2.692 \cdot 10^{-13}$	$3.609 \cdot 10^{-12}$
243.84	5	$4.685 \cdot 10^{-12}$	$1.307 \cdot 10^{-12}$	$1.437 \cdot 10^{-12}$	$3.535 \cdot 10^{-12}$
243.84	10	$4.001 \cdot 10^{-12}$	$1.279 \cdot 10^{-12}$	$1.405 \cdot 10^{-12}$	$3.462 \cdot 10^{-12}$
304.8	0	$2.968 \cdot 10^{-12}$	$1.294 \cdot 10^{-12}$	$6.666 \cdot 10^{-13}$	$2.317 \cdot 10^{-12}$
304.8	0.17	$1.805 \cdot 10^{-12}$	$1.295 \cdot 10^{-12}$	$1.128 \cdot 10^{-12}$	$2.351 \cdot 10^{-12}$
304.8	2.5	$2.667 \cdot 10^{-12}$	$8.551 \cdot 10^{-13}$	$9.452 \cdot 10^{-13}$	$2.308 \cdot 10^{-12}$
304.8	7.5	$2.595 \cdot 10^{-12}$	$8.304 \cdot 10^{-13}$	$9.151 \cdot 10^{-13}$	$2.247 \cdot 10^{-12}$
487.68	0	$2.239 \cdot 10^{-12}$	$3.891 \cdot 10^{-13}$	$2.853 \cdot 10^{-13}$	$9.255 \cdot 10^{-13}$
487.68	0.11	$9.615 \cdot 10^{-13}$	$6.889 \cdot 10^{-13}$	$1.053 \cdot 10^{-12}$	$9.408 \cdot 10^{-13}$
487.68	1.5	$1.063 \cdot 10^{-12}$	$3.435 \cdot 10^{-13}$	$3.854 \cdot 10^{-13}$	$9.191 \cdot 10^{-13}$
487.68	4.5	$1.055 \cdot 10^{-12}$	$3.382 \cdot 10^{-13}$	$3.760 \cdot 10^{-13}$	$9.139 \cdot 10^{-13}$

Table 4.13. Relative Neutron Flux Above 2.38 eV at Selected Positions Beyond the Experimental Assembly - Calculated by DOT/FALSTF

Position		Relative Flux			
r(cm)	θ (deg)	Case 4	Case 5	Case 6	Case 7
76.2	0	$4.27 \cdot 10^{-11}$	$1.22 \cdot 10^{-11}$	$9.13 \cdot 10^{-12}$	$4.6 \cdot 10^{-12}$
76.2	0.68	$2.94 \cdot 10^{-11}$	$2.66 \cdot 10^{-12}$	$5.09 \cdot 10^{-12}$	$1.01 \cdot 10^{-11}$
76.2	10	$6.0 \cdot 10^{-12}$	$5.13 \cdot 10^{-12}$	$3.55 \cdot 10^{-12}$	$4.5 \cdot 10^{-12}$
76.2	30	$4.6 \cdot 10^{-12}$	$4.57 \cdot 10^{-12}$	$2.83 \cdot 10^{-12}$	$4.1 \cdot 10^{-12}$
152.4	0	$5.85 \cdot 10^{-12}$	$1.57 \cdot 10^{-11}$	$1.48 \cdot 10^{-11}$	$1.61 \cdot 10^{-11}$
152.4	0.34	$2.42 \cdot 10^{-11}$	$2.57 \cdot 10^{-12}$	$3.70 \cdot 10^{-12}$	$4.56 \cdot 10^{-12}$
152.4	5	$1.93 \cdot 10^{-11}$	$1.82 \cdot 10^{-12}$	$1.11 \cdot 10^{-11}$	$1.59 \cdot 10^{-11}$
152.4	15	$1.68 \cdot 10^{-11}$	$1.52 \cdot 10^{-12}$	$9.67 \cdot 10^{-12}$	$1.50 \cdot 10^{-11}$
228.6	0	$5.70 \cdot 10^{-12}$	$1.90 \cdot 10^{-11}$	$7.63 \cdot 10^{-12}$	$7.9 \cdot 10^{-12}$
228.6	0.23	$1.56 \cdot 10^{-11}$	$1.79 \cdot 10^{-12}$	$2.31 \cdot 10^{-12}$	$2.63 \cdot 10^{-12}$
228.6	4	$9.4 \cdot 10^{-12}$	$8.56 \cdot 10^{-13}$	$5.30 \cdot 10^{-12}$	$7.9 \cdot 10^{-12}$
228.6	10	$8.4 \cdot 10^{-12}$	$7.44 \cdot 10^{-13}$	$4.77 \cdot 10^{-12}$	$7.6 \cdot 10^{-12}$
243.84	0	$6.96 \cdot 10^{-12}$	$2.12 \cdot 10^{-11}$	$9.37 \cdot 10^{-12}$	$7.0 \cdot 10^{-12}$
243.84	0.21	$1.42 \cdot 10^{-11}$	$1.66 \cdot 10^{-12}$	$2.27 \cdot 10^{-12}$	$2.4 \cdot 10^{-12}$
243.84	5	$8.1 \cdot 10^{-12}$	$7.29 \cdot 10^{-13}$	$4.6 \cdot 10^{-12}$	$6.9 \cdot 10^{-12}$
243.84	7.5	$7.6 \cdot 10^{-12}$	$6.56 \cdot 10^{-13}$	$4.2 \cdot 10^{-12}$	$6.5 \cdot 10^{-12}$
304.8	0	$3.54 \cdot 10^{-12}$	$4.90 \cdot 10^{-12}$	$1.99 \cdot 10^{-11}$	$4.7 \cdot 10^{-12}$
304.8	0.15	$1.05 \cdot 10^{-11}$	$1.25 \cdot 10^{-12}$	$1.58 \cdot 10^{-12}$	$1.75 \cdot 10^{-12}$
304.8	2.5	$5.8 \cdot 10^{-12}$	$5.31 \cdot 10^{-13}$	$3.28 \cdot 10^{-12}$	$4.7 \cdot 10^{-12}$
304.8	7.5	$5.0 \cdot 10^{-12}$	$4.70 \cdot 10^{-13}$	$2.82 \cdot 10^{-12}$	$4.7 \cdot 10^{-12}$
487.68	0	$1.01 \cdot 10^{-11}$	$3.39 \cdot 10^{-12}$	$1.74 \cdot 10^{-11}$	$2.0 \cdot 10^{-11}$
487.68	0.11	$5.54 \cdot 10^{-12}$	$6.61 \cdot 10^{-13}$	$8.84 \cdot 10^{-13}$	$9.8 \cdot 10^{-13}$
487.68	1.5	$2.7 \cdot 10^{-12}$	$2.39 \cdot 10^{-12}$	$1.6 \cdot 10^{-12}$	$1.9 \cdot 10^{-12}$
487.68	4.5	$2.2 \cdot 10^{-12}$	$1.89 \cdot 10^{-12}$	$1.2 \cdot 10^{-12}$	$1.9 \cdot 10^{-12}$

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Fig. 4.4. Diagram of Constant-Elevation Detector Traverse Showing the Arc Radius (r) and the Angle from the Axis (θ).

Table 4.14. Relative Neutron Flux Values at Selected Positions
Beyond the Experimental Assembly - Calculated by MORSE^a

Position		Relative Flux and Fractional Standard Deviation	
r(cm)	θ (deg)	Case 1	Case 2
152.4	0	1.47×10^{-6} , 0.07	5.4×10^{-9} , 0.70
152.4	5	1.16×10^{-15} , 0.23	8.7×10^{-17} , 0.29
152.4	10	3.71×10^{-16} , 0.17	8.6×10^{-17} , 0.29
152.4	15	2.80×10^{-16} , 0.19	8.0×10^{-17} , 0.29
304.8	0	6.27×10^{-7} , 0.07	2.3×10^{-9} , 0.73
304.8	2.5	5.68×10^{-15} , 0.31	4.2×10^{-17} , 0.27
304.8	5	2.86×10^{-16} , 0.19	4.1×10^{-17} , 0.27
304.8	7.5	1.64×10^{-16} , 0.16	4.0×10^{-17} , 0.27

^aTwo Monte Carlo runs were made for each case. For case 1, 10,000 source locations were selected over a disk of inner radius 7.62 cm and outer radius 50.8 cm in the first run and 5900 source locations were selected over a disk of radius 7.62 cm for the second run. For case 4, 10,000 source locations were selected over a disk of inner radius 3 cm and outer radius 50.8 cm for the first run, while 2600 source locations were selected over a disk of radius 3 cm for the second run.

Table 4.12 shows that the total fluxes for cases 4 and 6 are about equal to that for case 7 (background) when the detector is far outside the cell radius. The total flux for case 6 in some instances is below background because of lack of convergency of the thermal group ($E < 2.38$ eV). Table 4.13 shows the flux above 2.38 eV for case 6 to be at least six times background at all locations. However, for case 4, the flux at several positions is still barely above background.

Table 4.15 compares the DOT-calculated $E > 2.38$ eV and $E < 2.38$ eV fluxes at the end of the cell for the seven cases. The radius corresponds to the midpoint of the gap between the pin and the cell wall. Numbers computed with MORSE for cases 1 and 4 are given for comparison. The DOT results show that the flux above 2.38 eV for cases 1 to 6 is at least four orders of magnitude above that for case 7 (tank, cell, and collimator filled with water). Hence, either cell significantly increases

Table 4.15. Relative Flux at the End of a Cell at a Radius of 6.807 mm

Case	Relative Flux Calculated with DOT		Relative Flux and Fractional Standard Deviation Calculated with MORSE	
	$E < 2.38 \text{ eV}$	$E > 2.38 \text{ eV}$	$E < 2.38 \text{ eV}$	$E > 2.38 \text{ eV}$
1	1.429×10^{-6}	1.038×10^{-5}	1.2×10^{-7} , 0.601	7.06×10^{-6} , 0.059
2	2.999×10^{-6}	8.316×10^{-7}		
3	8.785×10^{-8}	7.744×10^{-7}		
4	2.261×10^{-7}	6.337×10^{-9}	1.6×10^{-8} , 1.0	2.51×10^{-9} , 0.3
5	1.396×10^{-7}	7.955×10^{-7}		
6	3.110×10^{-7}	1.225×10^{-8}		
7	5.117×10^{-9}	5.010×10^{-13}		

the transmission through the tank in the vicinity of the cell. The flux below 2.38 eV shows a smaller deviation.

A comparison of the DOT flux above 2.38 eV results with the MORSE results shows fairly good agreement for case 1, the DOT results being about 47% higher than the MORSE result. The agreement in the thermal range is not very good, possibly because of insufficiently deep penetration of particles into the tank. There is even less penetration with the collimator, so an even greater discrepancy is noted between the DOT and MORSE results for case 4, even for the high-energy neutrons. In this case the DOT flux above 2.38 eV is 2.5 times the MORSE value. The MORSE result is probably lower than the true flux because of an insufficient number of events occurring deep within the tank and in the vicinity of the cell.

4.3.1.2 Count Rates

Table 4.16 gives the counting rates with the 0.15-m (6-in.) Bonner balls expected if only the neutrons leaking from the cell are counted. Of course not all of these will be counted as the detector is moved away from the cell.

Table 4.17 gives approximate count rates for the 0.15-m (6-in.) Bonner ball at several positions for cases 4 through 7 and for the

Table 4.16. Expected Count Rate with 0.15-m (6-in.) Bonner Ball
Assuming All Neutrons Leaking from the Cell are Counted

Case	Count Rate (Counts min ⁻¹ V ⁻¹)
1	0.545
2	0.092
3	0.019
4	0.0023
5	0.020
6	0.0032

Table 4.17. Approximate Count Rates at Selected Positions

Position		Count Rate, Counts min ⁻¹ kg ⁻¹				
r (cm)	θ (deg)	Case 4	Case 5	Case 6	Case 7	
<u>6-in. Bonner Ball</u>						
76.2	0	3.0	36	3.5	1.0	
76.2	10	1.2	3.9	0.41	1.0	
76.2	20	1.2	3.7	0.39	0.99	
76.2	30	1.1	3.5	0.37	0.93	
152.4	0	2.1	30	2.4	0.33	
152.4	5	0.38	1.2	0.13	0.32	
152.4	10	0.37	1.2	0.13	0.32	
152.4	15	0.36	1.2	0.13	0.31	
228.6	0	1.3	21	1.4	0.15	
228.6	4	0.18	0.55	0.060	0.15	
228.6	7	0.18	0.55	0.060	0.15	
228.6	10	0.17	0.55	0.060	0.15	
243.84	0	1.2	19	1.3	0.14	
243.84	5	0.15	0.50	0.055	0.13	
243.84	7.5	0.15	0.49	0.055	0.13	
243.84	10	0.15	0.49	0.055	0.13	
304.8	0	0.88	14	0.99	0.088	
304.8	2.5	0.10	0.33	0.038	0.088	
304.8	5	0.099	0.32	0.038	0.088	
304.8	7.5	0.099	0.32	0.033	0.088	
487.68	0	0.46	7.7	0.51	0.033	
487.68	1.5	0.038	0.13	0.016	0.033	
487.68	3.0	0.038	0.13	0.016	0.033	
487.68	4.5	0.038	0.13	0.016	0.033	
<u>3-in. Bonner Ball</u>						
243.84	0	1.8	1.5	1.8	0.33	
243.84	5	0.39	1.2	0.13	0.33	
243.84	7.5	0.37	1.2	0.13	0.33	
243.84	10	0.37	1.2	0.13	0.33	
487.68	0	0.66	0.66	0.66	0.088	
487.68	1.5	0.11	0.33	0.044	0.088	
487.68	3	0.11	0.33	0.044	0.088	
487.68	4.5	0.11	0.31	0.044	0.088	
<u>10-in. Bonner Ball</u>						
243.34	0	0.099	4.7	0.12	0.020	
243.84	5	0.022	0.075	0.008	0.020	
243.84	7.5	0.022	0.075	0.008	0.020	
243.84	10	0.022	0.073	0.008	0.020	
487.68	0	0.039	2.0	0.039	0.006	
487.68	1.5	0.006	0.020	0.002	0.006	
487.68	3	0.006	0.020	0.002	0.006	
487.68	4.5	0.006	0.020	0.002	0.006	

0.076- and 2.4-m (3- and 10-in.) Bonner balls at a few positions. Again some of the case 6 count rates are lower than background because of insufficient convergence of the thermal flux.

4.3.1.3 Source Normalization

The source strength for the wide beam of the TSF is expected to be about 200 times that for the 0.387-m-diam (15.25-in.) beam. The old source strength was $4.5053 \times 10^3 \text{ n cm}^{-2} \text{ sec}^{-1} \text{ W}^{-1}$ or $5.3091 \times 10^6 \text{ n sec}^{-1} \text{ W}^{-1}$. The wide beam source strength is thus $1.0618 \times 10^3 \text{ n sec}^{-1} \text{ W}^{-1}$ or $1.3097 \times 10^5 \text{ n cm}^{-2} \text{ sec}^{-1} \text{ W}^{-1}$ distributed over a 0.10-m-diam (4-in.) disk. All results in Tables 4.12 through 4.15 should be multiplied by the appropriate wide-beam factor and the reactor power in watts to obtain absolute values.

4.3.1.4 Conclusions

In all cases it was seen that the $E > 2.38 \text{ eV}$ leakage from the cell was greater than that from the tank. And in all cases the $E < 2.38 \text{ eV}$ leakage from the system was greater than the $E > 2.38 \text{ eV}$ leakage, with the tank being the main source of the $E < 2.38 \text{ eV}$ leakage. The addition of poison in the water in the tank would significantly reduce the thermal leakage and thus make leakage from the cell greater than that from the tank.

It was also noted that the $E > 2.38 \text{ eV}$ leakage from the cell for case 5 was virtually unchanged from that for case 3. That is to say, only a small increase in leakage occurred when the iron pin was replaced by the UO_2 pin. Perhaps this is due mainly to the fact that with the partial collimator, most of the leakage can be attributed to uncollided neutrons. On the other hand the $E < 2.38 \text{ eV}$ leakage from the tank and the system itself was more than doubled. This could be due to the fast neutrons generated in the UO_2 pin being thermalized near the end of the tank and emerging as thermals. If the increase in the leakage can be attributed to fissions in the UO_2 pin, the results show that in the $E > 123 \text{ keV}$ range the additional leakage is split equally between the cell and tank. In the intermediate energy range, the additional leakage

from the tank is 2.5 times that from the cell. This may indicate a significant coupling among cells in a rod array, in that a significant number of neutrons produced in one cell could then be expected to stream out through surrounding cells.

4.3.2 Prealysis of the Full-Array Streaming Experiment

Other efforts were directed toward the preanalysis of the GCFR full-array streaming experiment, including the mockup of the geometry of the full-rod-array experiment for a MORSE calculation. Because of reflective boundary requirements, this is requiring the designation of an exceptionally large number of bodies. The setup for the problem is therefore quite lengthy and complex, and it is anticipated that the calculation will require a large core storage area and a long running time. DOT calculations of the homogeneous full-array experimental configuration, both forward and adjoint, will therefore be made to assist in the MORSE Monte Carlo calculation of the heterogeneous full array. Adjoint DOT calculations have been made with a 0.15-m (6-in.) Bönner ball response as the source at two locations beyond the tank. In these calculations the UO_2 rods were homogenized radially within the full array configuration, and work has continued on the MORSE full-array configuration. The geometry package has been checked out for the MORSE run, and the cross sections have been prepared and checked out with short runs with the current source representation. Next the forward and adjoint rod calculations will be applied to aid in biasing the MORSE full-array heterogeneous calculation, and checkout of the calculation will continue, leading towards performing the full-array calculation when a suitable source representation is available.

The major effort in June was directed toward obtaining a reasonably representative TSR-II source calculation for use in analyzing GCFR pin and full-array streaming experiments. This was necessary because of the change from the small beam to the new broad beam configuration for the source. The problem involved converting angular fluxes on the boundary of the TSR-II reactor to a source that could be used in DOT. Three alternatives were to: (1) convert the angular fluxes to an equivalent point source

at the spherical center and then generate a first collision source for DOT, (2) use the unaltered angular fluxes to generate a first collision source for DOT, or (3) convert the angular fluxes to a disk source. The latter approach was taken. The source calculation was carried through the spectrum modifier. A preliminary calculation shows a 0.15-m (6-in.) Bonner ball measurement to be about 40-50% higher than the calculated result using the current source presentation.

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5. DYNAMIC MODELING OF THE GCFR STEAM GENERATOR

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The final refinements to a dynamic model of the GCFR steam generator were completed. The hybrid computer model is a continuous space, discrete time model of the feedwater flow control valve, the steam generator, and the helium circulator control valve. The variables used in the calculations in this model are updated at 77 space points in the steam generator, making it a very fine-structured model. The profiles of the steam generator variables of interest were generated for design point, steady-state operation, and they were very nearly the same as those generated by GA with its model. The same test transient was run using this model, and GA's overall system model and the results were in fair agreement.

The work was terminated before obtaining significant results.

GAS-COOLED REACTOR PROGRAMS

REACTOR DEVELOPMENT	NO
REACTOR DESIGN	NO
REACTOR OPERATION	NO
REACTOR MAINTENANCE	NO
REACTOR SAFETY	NO

GAS-COOLED REACTOR PROGRAMS	
REACTOR DEVELOPMENT	NO
REACTOR DESIGN	NO
REACTOR OPERATION	NO
REACTOR MAINTENANCE	NO
REACTOR SAFETY	NO
REACTOR ECONOMICS	NO

 