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GAS-COOLED REACTOR
PROGRAMS

GAS-COOLED FAST REACTOR PROGRAM
ANNUAL PROGRESS REPORT
FOR PERIOD ENDING DECEMBER 31, 1977

Uri Gat
P. R. Kosten

ORNL-5426
Dist. Category UC-77

Contract No. W-7405-eng-26

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FOR PERIOD ENDING DECEMBER 31, 1977**

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Date Published: August 1978

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SUMMARY

INTRODUCTION

The GCFR Program at ORNL covers three work areas: the Core Flow Test Loop (CFTL), Shielding Studies, and Prestressed Concrete Reactor Vessel (PCRVR) Closure Studies. The CFTL will simulate the behavior of GCFR fuel elements under steady-state and transient power and cooling conditions. The associated CFTL Program will provide engineering design information relative to the expected thermal, hydraulic, and structural behavior in GCFR fuel elements. The GCFR Shielding Studies supply analytical and experimental information related to the proper shielding design of GCFRs, while the PCRVR Closure Studies provide analytical and experimental information related to the engineering design of closures for PCRVR cavities.

1. GCFR CORE FLOW TEST LOOP

The CFTL is a high-temperature, high-pressure, fast-transient, out-of-reactor helium circulating loop. Its data acquisition system is commensurate with the need to follow and record the fast transients associated with GCFR upset, emergency, and faulted conditions. The GCFR fuel and blanket pins are simulated with electrically heated simulator rods, providing an exact simulation from the cladding outward. During the reporting period, the CFTL Program continued its development activities and also completed the advanced conceptual design stage. A detailed cost estimate was prepared, based on the advanced conceptual design. A directive was received from the Department of Energy (DOE) on January 9, 1978, to proceed with Title I and Title II design. Purchase of long-delivery capital equipment will proceed as funds are made available.

Conceptual design of the test bundle and test vessel has continued. Gray-Loc blind flanges will be used as closures for the top and bottom of the test vessel. The inlet plenum will be designed to ensure fully mixed, uniform flow of helium coolant upstream of the rod heating element, which simulates GCFR fuel. The outlet plenum will be designed to ensure that the pressure boundary temperatures do not exceed the design value temperatures and that attenuation and coolant flows are mixed in a controlled manner. The test vessel exit is provided with Inconel liners to reduce thermal stresses during high-temperature cycling.

A flange closure development test was conceived as a verification of the design of the combination test-vessel flange and test bundle tube sheet at typical CFTL operating conditions. The primary purpose is to determine experimentally the strength of the tube sheet (the thinned portion of the blind flange) at the fuel rod simulator penetrations and the rod-cladding-to-tube-sheet joints. Another feature of the test is to evaluate the effectiveness of a proposed cooling scheme for the rod and power-supply electrical leads and their connectors and to correlate

the lead operating temperatures with predicted values. Four methods of joining the rod cladding to the tube sheet are being considered, and evaluation tests are under way. Development of electrical connectors is required because of the high currents (potentials to 300 V) and the close spacing of the rods. Development has concentrated on methods of attaching leads to the concentric rod conductors above the tube sheet and of predicting operating temperatures.

The evaluation of candidate ac- and dc-power supplies for the test bundle was completed. An ac linear multipower-level power supply (LMPL) having zero-firing silicon controlled rectifiers (SCRs) was selected. The ac system was chosen primarily because of less induced electromagnetic noise and the ability to filter ac leakage currents from rod internal thermocouples.

An active program on fuel rod simulator development was in progress throughout this year. Seven prototype rods were fabricated, and four underwent operational testing at sheath temperatures up to 1260°C. Operational data were obtained on both boron nitride (BN) electrical resistance and dc leakage to internal thermocouples during single rod tests. Problems with impurities in powdered BN insulation were encountered and corrected. The fabrication of high-density pressed BN preforms for use in high-performance fuel rod simulators was extended from the laboratory to commercial production. Precise techniques for volume production of variable-width-ribbon heating elements were perfected. One rod was fabricated successfully with a platinum-8% tungsten heating element in an attempt to obtain a high-performance rod capable of operation near to clad melting. At DOE-HQ direction, further work on the advanced rod was stopped, and present development efforts are now centered on a lower-temperature reference rod having a Nichrome V heating element.

Fabrication of a prototype of the helium circulator that features gas film-bearing lubrication and hermetic construction was continued under a CPFF contract to about 70% completion. Problems in both funding and component fabrication have delayed the completion date of prototype testing to the third quarter of FY 1979. Fabrication of the solid motor rotor and the stator was completed in July 1977, and engineering evaluation of motor tests was received in December 1977, thus completing the motor subcontract. Design, fabrication, and acceptance testing of a solid-state, variable-frequency power supply, which will be used in development testing of the prototype circulator, was completed.

The configuration of the CFTL fast-scan data acquisition system was determined. It will consist of a computer-based data/acquisition system and an automated calibration and verification system. The system will be capable of interrogating as many as 640 measurement channels at a total rate of 20,000 data points per second.

Work continued on characterization of small-diameter (0.5-mm OD) sheathed thermocouple materials to be used in the fuel rod simulators. The effort has included (1) analysis of errors, (2) determination of

reliability under CFTL conditions, and (3) collection of data on several combinations of thermocouple and sheath materials. During tests up to 1150°C, Inconel 600 sheaths showed much less decalibration of type K thermocouple material than stainless steel sheaths. Limited testing was also done on thermocouples using noble metal thermoelements and sheaths, but this work was halted at the direction of DOE-HQ.

A mechanical equipment arrangement and a primary helium piping layout were completed. Limited building modifications will be required to permit test bundle handling and equipment maintenance and to provide support for heavy equipment items. Preliminary designs of two helium-to-air heat exchangers and related air-handling equipment were completed. Dynamic analysis of the layout is in progress for a wide range of steady-state and transient operations to demonstrate that selected equipment will meet CFTL requirements. Preliminary specifications for the major components of the electrical power distribution system were prepared, including 13.8-kV switchgear, several transformers, and related 480-V switchgear. Conceptual instrument-application diagrams were completed for both the loop and test vessel, and preliminary specifications for the instrument components were completed. A tentative control room layout was completed to establish space requirements for signal conditioning cabinets, instrument cabinets, and data acquisition equipment.

Thermal flow analysis of the test bundle has been initiated. Several computer codes were received from the General Atomic Company (GA) and modified as required for use with the IBM system at ORNL. Structural analysis of the test bundle is also under way at ORNL to determine a suitable computational method.

2. SHIELDING STUDIES

The emphasis in the GCFR Shielding Studies during 1977 was on the neutron streaming experiment at the Tower Shielding Facility to determine the effectiveness of the grid plate shield design. Other parts of the program covered the analysis of grid plate damage and radial as well as upper and lower axial shield designs. Calculations were performed to analyze advanced core designs for alternate fuel cycles. A cooperative program between the Swiss Federal Institute for Reactor Development (EIR) and ORNL was initiated in which ORNL is assisting with analysis in support of an experiment being carried out at the Proteus reactor facility at EIR.

The grid plate shield experiment was designed to allow flexibility of measurement configurations. A test program for the grid plate shield experiment that defines several priorities was laid out. The program makes use of various possible experimental configurations to verify a variety of shielding parameters. To minimize the direct neutron stream from the source reactor, a shadow shield was constructed. A systematic series of measurements was performed, starting with the background by applying the shadow shield and concrete block only and then adding successive components between any two series of measurements. The results show the anticipated streaming effect between subassemblies.

superimposed with a smearing effect that depends on type and size of detector. In support of the experiment, calculations were made to determine their sensitivity to various parameters. The 51-group GCFR library was compared to the 7-group cross section. The impact of the water content of the concrete was calculated, as was the source of the neutrons and their energetic spectrum distribution. Reading predictions of the different sensors were made and later compared with results. Scattering of neutrons and the effect from various physical positions on a detector reading were calculated and plotted in contribution flux three-dimensional diagrams. Isofission and isoflux plots were generated and provided an important contribution to the understanding of the streaming effect. Comparative calculations of the experiment and of the GCFR design led to the conclusion that the experimental simulation of the GCFR is reasonable.

Analysis in support of the GCFR shield design led to revised estimates of the grid plate damage. The sensitivity analysis revealed both the effect of changes in the density of boron carbide on shielding and the effect of varying interassembly gap on grid plate damage. A revised neutron source for the lower and upper axial shields calculations was determined. In refining the calculational methods, various models and computations were compared. In particular, the relative merits of the heterogeneous and the homogeneous models were evaluated. The effect of different core designs was determined, and the beginning-of-life and an equilibrium core configuration were modeled and their impact on shielding evaluated. The buildup of plutonium in the blanket was an important consideration in going to the equilibrium core.

In calculating alternate fuel cycles, emphasis was on the denatured fuel and its use in a GCFR. The initial scoping calculations were based on a 1.2-GW reactor. Breeding ratios and doubling time for a variety of fuel-and-blanket material combinations were calculated. A reactor energy strategy, using the energy center concept with GCFRs as fuel-isotope transmuters and a variety of other reactors as prime energy producers and fuel burners, provided a proliferation-resistant scheme.

3. PRESSURE VESSEL AND CLOSURE STUDIES

Activities in the Pressure Vessel and Closure Studies in 1977 concentrated on four areas: (1) the design of as well as the simulation of mechanical properties of components for the 1:15-scale central reactor cavity closure plug model; (2) the testing of the 1:15-scale steam generator cavity closure plug model; (3) the fabrication, instrumentation, and testing of a half-thickness version of the 1:15-scale steam generator cavity closure plug model; and (4) the design and procurement of equipment and facilities to accommodate 1:4-scale closure models and the 1:10-scale PCRV model.

The design of the 1:15-scale central cavity closure is a rather complex one because it deals with 237 fuel-element and 20 control-rod tubes as well as interlacing steel gussets and top and bottom liners. Materials for the model were procured and treatment specified. Instruments for the model and the test fixture were laid out. Before implementation of the model, the design was changed, and further activity was delayed until redesign is complete.

Two 1:15-scale steam generator cavity closure models were tested during the reporting period. One of the models is the full-thickness model, while the second is a so-called half-thickness model. Testing consisted of exposing the model to ten prespecified pressurization cycles. The last pressurization of the full-scale model was raised in steps to 75.8 MPa, which is approximately 7.5 times the design pressure. The model did not fail, nor was any distortion visible. The half-thickness model, basically the same as the full-thickness model reduced to half depth with the shear console removed, was analyzed to fail at about 75 MPa. This second model underwent the same test program as the first model; final pressurization was raised to 99 MPa, at which pressure the model failed by a punching shear.

Conceptual design of the 1:4-scale test fixture was completed. Further work on the 1:4-scale test fixture has been suspended because of pending revisions of the basic criteria and test program.

1. GCFR CORE FLOW TEST LOOP

Uri Gat, R. E. MacPherson, and A. G. Grindell

1.1 INTRODUCTION — A. G. Grindell and W. R. Huntley

The program for the Core Flow Test Loop (CFTL) at ORNL for the gas-cooled fast breeder reactor (GCFR) includes (1) construction of the CFTL; (2) construction of test bundles that simulate GCFR core assemblies and their operation in the CFTL at steady-state and normal, upset, emergency, and faulted conditions anticipated for the GCFR; (3) development tasks supporting the designs of the CFTL and the test bundles; (4) analysis supporting the construction and operation of the CFTL and the test bundles; and (5) the acquisition and analysis of programmatic test data taken during test bundle operation.

Progress during the year with the design, development, and analysis tasks supporting the advanced conceptual design of the CFTL and with the design and development work supporting the design of the test bundles is discussed in this report.

1.1.1 Objectives

The viability of the GCFR depends strongly on the steady-state characteristics and the transient responses of the core assemblies, according to the program plan¹ prepared for the GCFR CFTL by the General Atomic Company (GA). Information on heat-transfer and pressure-drop characteristics of its core during steady-state operation is important to the design and costs of the GCFR. Information on the responses of the core assemblies to normal, upset, emergency, and accident transients anticipated for the GCFR is important to determining the design and safety margins of the core.

The CFTL, which is part of the GCFR Program at ORNL, will be designed to provide this information. It will be a high-pressure, high-temperature, out-of-reactor circulating helium system that will be designed to supply the test requirements of the program plan and GA's CFTL test specifications² for individual CFTL test bundles that model GCFR core assemblies.

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The requirements include test bundle power and heat removal; helium coolant flow rate, pressure, and temperature; and levels of helium purity and injected gaseous impurities. It will be designed to simulate the test conditions specified for GCFR steady-state operation; normal, upset, and emergency transients; and the rapid helium depressurization associated with the GCFR design-basis accident. It will be designed to impose on each installed test bundle either uniform or skewed power distributions as required.

A test bundle will consist of electrically heated fuel rod or blanket rod simulators arranged to model a segment of a fuel or control rod assembly or a full-size blanket assembly in the GCFR core. Temperatures and pressure-drop characteristics will be measured. The effects of thermal and mechanical interactions between the rods and the grid spacers and hexagonal duct, caused principally by transient operations and core undercooling, will be determined both visually and by measurements during out-of-loop examination. Image-enhanced fluorescent radiography is being evaluated as a means of observing test bundle movement or deformation during operation.

Steady-state operation of test bundles in the CFTL, which will include flat or skewed power distribution across test bundles at prescribed power levels and slopes, will be conducted to (1) verify heat-transfer and pressure-drop 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 and acoustic characteristics, and (5) explore local flow blockage to detect blockage and to measure thermal and hydraulic effects of blockage.

Transient operation of the CFTL will include prescribed variations with time in test bundle helium coolant flow rate and power from an initial flat or skewed power distribution to simulate normal, upset, and accident transients as well as helium pressure and bundle flow rate and power to simulate depressurization accidents. Such operation will be conducted to (1) verify the ability of GCFR core assemblies to withstand normal and emergency transients; (2) determine design and safety margins

of the core assemblies under extreme undercooling, overcooling, and depressurization transients; (3) explore the operation of core assemblies for structure-related behavior and deficiencies; and (4) obtain information on the interaction between fuel rod and spacer grid and vibration and performance data for outlet gas temperature sensors, flow control orifices, and control rods that may be used in GCFR core assemblies.

1.1.2 Test Capability

The CFTL will have the capability to provide a maximum of 4100 kW of electrical power to a test bundle. Regulation of bundle power will be based on zone control of individual rows of rods from 100 ± 1 to 0.5%. The lower limit of power controllability is required in special tests of single fuel rods in a 37-rod bundle in which power will be reduced to about 300 W/rod. Maximum rod power is 45 kW/rod for blanket rods and 38 kW/rod for fuel rods. Each row of rods (zone) will be parallel to one face of the bundle's hexagonal configuration. In a flat power distribution, the same amount of electric power will be supplied to each rod in a test bundle. In a skewed power distribution, although the same value of power is supplied to each rod in a single zone, the CFTL will have the capability to vary the value of total power in each of the several zones. This capability will provide a relatively smooth linear power distribution, having a slope of 0 to 4% per transverse centimeter 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 have the capability to be reduced from 100 to 10% in 1, 2, 4, or 16 s. Figure 1.1 shows a fast transient of GCFR core power during a reactor trip.

The CFTL will be designed to provide helium coolant to the test bundle at 9- to 10.3-MPa inlet pressure and 325 to 350°C inlet temperature at mass flow rates up to 3.2 kg/s, which is sufficient to remove heat from a 91-rod fuel test bundle. During a flow transient, the helium coolant flow rate will be reduced from 100 to 10% in time periods of 1, 2, 4, or 16 s. Figure 1.2 shows a typical helium coolant flow transient during a GCFR reactor scram.

Fig. 1.1. Core Power vs Time During a GCFR Reactor Trip. Gravity drop of primary rods. Source: H. C. Hopkins, Jr., *Program Plan for GCFR Flow Test Loop*, General Atomic Co., GA-A13080, Aug. 9, 1974.

Fig. 1.2. Flow Rate of Core Coolant Helium vs Time During a GCFR Reactor Trip. Source: H. C. Hopkins, Jr., *Program Plan for GCFR Flow Test Loop*, General Atomic Co., GA-A13080, Aug. 9, 1974.

The loss of helium coolant through a leaking cavity closure in the prestressed concrete reactor vessel (PCRV) is currently the design basis accident (DBA) for the GCFR. A leak size of 0.048 m^2 (75 in^2) is the design basis depressurization accident (DBDA). In the DBDA a loss-of-pressure signal will scram the reactor. It will then be necessary to remove decay heat from the core in a satisfactory manner to avoid a nuclear radiation insult to the public health and welfare. The CFTL will be designed to simulate GCFR depressurization accidents. Figure 1.3 shows the calculated helium pressure during typical depressurizations as anticipated for the GCFR.

Fig. 1.3. Coolant Pressure During Depressurization of GCFR. Source: H. S. Hopkins, Jr., *Program Plan for GCFR Flow Test Loop*, General Atomic Co., GA-A13080, Aug. 9, 1974. Reprinted by courtesy of General Atomic Co.

The GCFR helium flow rate and reactor power during the 75-in.² depressurization is shown in Fig. 1.4 (as provided by the General Atomic Company).³ The CFTL will be programmed to provide a similar mass flow rate through the test bundle during this event. The sharp drop in flow

Fig. 1.4. Core Power, Flow, Heat Transport Rates, and Coolant Temperature Rise During a 75-in.² Depressurization Accident. Best estimate model with three loops operational. Source: General Atomic Company, *Gas-Cooled Fast Breeder Reactor Preliminary Safety Information Document*, GA-10298, Amendment 7, Feb. 23, 1976. Reprinted by courtesy of General Atomic Co.

rate at an elapsed time of about 85 s is caused by an assumed transfer of flow convection duty from the GCFR main circulators to the auxiliary circulators. Reactor power initially drops more rapidly than the flow rate so that overcooling of the test bundle will occur first, followed by overheating as the heat transport rate drops below the reactor power decay curve.

The CFTL will also be designed to permit operation of test bundles at high temperatures approaching melting of the rod 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 capable of operating at the rate of 20,000 samples per second to acquire data during fast transients. Table 1.1 presents additional steady-state operating requirements for the CFTL.

Table 1.1. Steady-State Operating Requirements
in the CFTL

Coolant	Helium
Design pressure, MPa (psia)	11.8 (1715)
Maximum operating pressure, MPa (psia)	10.3 (1500)
Normal operating pressure, MPa (psia)	9 (1300)
Minimum operating pressure, MPa (psia)	0.15 (21.8)
Test section	
HE/P test section	0.034
GCFR fuel assembly ΔP , kPa (psi)	307 (44.5)
Inlet temperature, °C (°F)	
Nominal	320 (610)
Minimum	260 (500)
Maximum	350 (660)
Outlet temperature, °C (°F)	
Nominal	550 (1020)
Minimum	260 (500)
Maximum	593 (1100)
Approach to clad melting	1370 (2500)
Maximum total helium flow, kg/s (lb/hr)	3.2 (7)
Helium flow per rod	
Core maximum, g/s (lb/hr)	30.5 (242)
Blanket maximum, g/s (lb/hr)	27 (215)
Core maximum linear power rating, kW/m (kW/ft)	33.6 (10.2)
Axial maximum-to-average power ratio	1.21
Maximum transverse fuel power gradient, %/cm (%/in.)	4 (10)
Minimum transverse power gradient, %/cm (%/in.)	0 (0)
Total simulated fuel rod length, mm (in.)	2220 (87.4)
Simulated core length, mm (in.)	1130 (44.5)
Axial blanket length (each end), mm (in.)	450 (17.7)
Maximum power per fuel rod, kW	38
Nominal power per fuel rod, kW	28
Simulated radial blanket rod length, mm (in.)	2220 (87.4)
Power per blanket rod, kW	45

1.1.3 Documentation and Cost and Schedule Estimates – H. L. Mitchell and W. R. Huntley

The CFTL work breakdown structure (WBS) and milestone event schedules were formulated for use in compiling a cost estimate and in programming the E-Z PERT scheduling and reporting system. The schedules were also used in preparing the Preliminary Proposal and the Request for Directive, and in preparing various funding and scheduling scenarios. From the WBS and milestone schedules, an initial E-Z PERT base schedule was made. In addition to these efforts, other reports such as the

Schedule 47, the Engineering Progress Monthly Histogram, and WBS and milestone schedules for the CFTL Program were generated.

The first complete issue of the System Design Description was completed in December 1977 and forwarded to the Department of Energy for approval.

1.2 DEVELOPMENT ACTIVITIES - Uri Gat and A. G. GrinJell

1.2.1 Test Bundle Development - H. C. Young

1.2.1.1 Flange Closure Test - C. W. Collins and H. C. Young.

The flange closure development test was conceived as a verification of the design of the test vessel flange under actual CFTL operating conditions. The test is envisioned as a test bed to prove selected test bundle and test vessel design concepts. The primary purpose is to determine experimentally the strength of the thinned, tube sheet portion of the blind closure flange at the top end of the test vessel and the rod-to-tube-sheet weld joints. Another important feature of this test is to evaluate the effectiveness of a system scheme for the rod and power-supply electrical leads and associated connectors and to correlate conductor lead operating temperatures with those predicted by thermal analyses.

During the past year, a preliminary design of the flange closure test was completed, and a cost estimate for its design, construction, and operation was prepared. The preliminary design consisted of assembly drawings of the flange welding hub with pipe cap (the pressure vessel), the machined tube sheet, shortened versions of the fuel rod simulators, rod leads, rod lead cooling scheme, rod power, and thermocouple disconnects. Initial detailed drawings were prepared for the tube sheet, pressure vessel, rods, rod leads, and disconnect parts.

All design work on the flange closure test will be discontinued during FY 1978 as a result of development funds being shifted to increase the design effort on the loop construction activities as directed by DOE-HQ.

1.2.1.2 Rod-to-Tube-Sheet Seal Evaluation - C. W. Collins and H. C. Young

Four methods for joining the sheaths of the test bundle rods to the test bundle tube sheet are being considered. These methods include electron beam welding, tungsten inert-gas (TIG) welding, brazing, and sealing with a ceramic cement.

Drawings were prepared, and parts were machined for fabricating a number of test capsules to test these methods. Each capsule was made up of seven dummy rods to be joined by a test method to one face of a tube sheet having a 2-in., schedule-40 pipe cap welded to the opposite face to form a test capsule. The completed capsules will be heated to 399°C (750°F), pressurized to 4.93 MPa (700 psig), and then temperature and pressure cycled to evaluate the rod-to-tube-sheet joints. All material is 300-series stainless steel. For the welded rod-to-tube-sheet joints, one flat surface of the tube sheet was overlaid with type 308 stainless steel, seven holes were drilled through the tube sheet to provide a close fit on the dummy rods, and weld trepans were then machined into the overlaid surface concentric with each hole, the trepan lip thickness being equal to the cladding thickness of the dummy rod. The weld overlay is to prevent welding cracks at the trepanned surface.

Four 0.5-mm-diam wires made of annealed type 304 stainless steel, each being 355 mm (14 in.) long, were inserted into the end of each dummy rod to simulate the thermocouples in a fuel rod simulator. The wires were stored by being closely wrapped on the outer diameter of the simulated electrical extension lead to mock up realistically the dimensional constraints during electron-beam or TIG welding. In an actual CFTL test bundle, four thermocouples in a fuel rod simulator must be stored similarly during the process of joining the rod cladding to the tube sheet.

Design of a small test facility for simultaneous temperature and pressure cycling of six test capsules was completed. Construction of the facility is nearly complete.

Two test capsules, including the seven dummy rods sealed into the tube sheet with ceramic cement, were prepared by the ORNL Metals and Ceramic Division. Final leak testing revealed that the cemented seal was porous and leaked. After evaluating this problem, the Metals and Ceramics

Division reported that this cement makes a very strong metal-to-metal joint; however, voids develop when the water in the cement mix is driven off during curing by heating to about 250°C. This porosity problem is particularly critical in cases where a relatively thin layer of cement is used, as in our application. No further development of the ceramic cement seal method for the CFIL tube-sheet-to-rod seal is contemplated at this time.

Initially several test assemblies were to have been sealed by brazing. The Metals and Ceramics Division does not recommend the use of brazing on full-size tube sheets, in recognition of the difficulties of adequately and uniformly heating the tube sheet and the many associated rods during the brazing process. Although the assemblies could probably be brazed satisfactorily, the brazing process would not be representative of that needed for a full-size tube sheet. Further development of the braze method has been postponed and may be dropped entirely pending results of the electron-beam and TIG welding evaluations.

Two test assemblies for evaluating the electron-beam welding process have been installed in the test facility, and pressure-temperature cycling tests will be started soon.

The Metals and Ceramics Division is now preparing fixturing for welding two test assemblies by the TIG welding process.

1.2.1.3 Electrical Connectors - J. A. Seneker and C. F. Holloway

Development of electrical connectors for the fuel rod simulators (rods) has been concentrated on (1) methods of attaching the power supply leads to the two concentric conductors in the rod, which extend above the tube sheet, and (2) thermal analyses to predict temperatures of the rod in the tube sheet region and in the connectors.

Design concepts for attaching the leads to the rod were prepared with two methods of attachment being considered: one method would braze the lead to the rod by using a modified TIG welding apparatus, and another method would use a compression ring to clamp the lead to the rod conductor. The ring would be fabricated of a material, Nitinol, that contracts when heated above its transformation temperature (usually less than room temperature). Drawings for connectors made to these two

designs were prepared, and development, fabrication, and assembly of the connectors were begun; diversion of development funds to loop design, however, halted the activity.

A thermal model of the leads and connector region was set up, and various designs were analyzed to predict temperatures in the rod and leads. The model was prepared using the HEATING 5 computer program to simulate the center rod of a 91-rod fuel bundle, the effective power generated by the surrounding 90 rods, and the remainder of the tube sheet. Analyses of several variations of the two connector methods were made to determine the optimum connector design for minimizing temperatures in the rods and leads. These analyses evolved the two designs currently being considered and indicated that the Nitinol connector should probably be restricted to the center-rod connector because of excessive temperatures elsewhere.

1.2.2 Leakage of dc Current to Thermocouples Within Fuel Rod Simulators - W. R. Huntley

1.2.2.1 Introduction

This test was operated to determine if dc leakage from the heating element to internal thermocouples within fuel rod simulators caused significant temperature measurement errors at the operating temperatures proposed for the CFTL. The test results were used to aid in the choice between an ac- or a dc-power supply for the CFTL test bundles. Three prototypic rods were tested for leakage, and first signs of significant errors were found at a temperature of 1230°C (2250°F). Evidence of dc leakage at these high operating temperatures was one factor in the final decision to use ac power for the test bundles because the CFTL rods may ultimately be operated at temperatures greater than 1230°C to simulate clad melting within a reactor core. Current leakage to rod thermocouples from an ac-power system is, of course, less serious than dc leakage because the ac leakage can be filtered conveniently.

1.2.2.2 Test Description

The test apparatus consisted of a single rod suspended vertically in a 20-cm-OD vessel. The test rods were designed to produce a uniform

heat flux and had boron nitride insulation and up to four internal thermocouples. The rods, which were 7.5 mm (0.294 in.) in OD with a 1130-mm-long (44.5-in.) heated section, were connected to a dc-power supply. Two CFTL prototypic rods were tested in vacuum, and one was operated in 2 atm of static helium. Operating heat fluxes were relatively low (a maximum of 7 kW per rod at 1200°C) because heat was transferred to the walls of the air-cooled enclosing vessel only by natural convection and radiation. Current leakage to internal thermocouples was recorded with a fast-scan data acquisition system (DAS) at a rate of 10,000 points/s. A typical test consisted of adjusting the rod temperature to a desired steady-state level and then interrupting the dc power for a few seconds while monitoring rod power and all thermocouple signals with the DAS. Tests at each temperature were repeated with a reversed dc potential to define more accurately effects on the thermocouple output. Electrical leakage effects could easily be discerned from signal changes caused by rod cooling with the high-speed DAS during power interruptions and potential reversal. The internal rod temperature measurements were compared with those of two externally attached type S thermocouples and an optical pyrometer to verify operating temperatures. A simplified schematic of the test apparatus is shown in Fig. 1.5.

1.2.2.3 Test Results

The first test rod (GLR-L-1P) featured a Nichrome V heating element designed to produce a uniform heat flux and a nickel central conductor with copper extension at the open end. This rod was operated in a vacuum of about 10 μ m of mercury provided by an untrapped mechanical roughing pump. Prior to operation the rod was intentionally oxidized by heating in air for 3 h at 700°C to improve radiation heat transfer (emittance) to increase the power level at which the rod would operate within the vacuum vessel. The rod was tested at temperatures of 700, 800, and 900°C with no measurable dc leakage on the temperature signals from four different 0.5-mm-OD, stainless-steel-sheathed, type K internal thermocouples. Tests of the rod at 1000°C were initiated but not completed due to an unexpected loss of oxide film on the heater surface. The loss of oxide is believed to be caused by vaporization of the film

Fig. 1.5. Simplified Schematic of dc Leakage Test for CFTL.

at 1000°C. This loss resulted in a lowered surface emittance that, at constant rod power, increased the surface temperatures until rod failure occurred due to a hot spot near the center of the heated length.

The loss of oxide film at 1000°C was investigated further, using spare rods from the Thermal Hydraulic Out-of-Reactor Safety Facility (THORS). These THORS rods had Kanthal heating elements; they were similar in clad material, diameter, and length to the prototypic CFTL rods and also fitted easily within the test vessel. However, these particular THORS rods had no internal thermocouples; therefore, temperatures along the rod were measured only by an optical pyrometer at four view ports through the test vessel walls. Operation of the first pre-oxidized THORS rod showed stable operation at 700, 800, and 900°C, but noticeable loss of oxide film occurred after only about 30 min of operation at 1000°C. Clad melting occurred at a constant 2.8-kW power input due to the lowered rod surface emittance as the oxide film was

lost. The test was terminated when clad melting was visually observed through a view port.

Another spare preoxidized THORS rod was tested in 2 atm of helium in the test vessel (this was the maximum allowable pressure for the vessel) to compare loss of oxide film under an environment more related to that expected in the CFTL. This rod was modified by attaching two type S thermocouples to the sheath exterior for comparison with the optical pyrometer reading. The oxide film was stable at 900°C over a 10-h period in helium. However, after 100 min at 1000°C, the rod temperature started to climb exponentially as noted by the type S surface thermocouples. The optical pyrometer did not indicate a temperature increase because the instrument is not self-compensating for emittance changes; in fact, the pyrometer-indicated temperature actually was somewhat lowered because of the large reduction in emittance of the rod surface as the oxide was removed. The rod was de-energized at 1070°C to prevent failure, and the test was halted. It was concluded that the helium environment lessened oxide removal compared to a vacuum; however, the removal rate in helium was still too rapid to allow adequate testing of the rods at temperatures above 900°C. Therefore, we investigated several high-emissivity surfaces such as grit blasting and iron titanate before adapting a ZrO_2 coating for the remainder of the test rods. The ZrO_2 coating was applied to the type 316 stainless steel rod cladding by first vapor blasting, next applying a substrate of tantalum, and finally plasma spraying a 0.08-mm-thick (~3 mils) layer of ZrO_2 .

It is recognized that CFTL test bundle rod surfaces will not be intentionally preoxidized before operation; nonetheless, the rods will attain some oxide film at normal operating conditions. It is likely that a loss of surface oxide film will also occur in the CFTL test bundles when they are operated above 1000°C in helium with low water content. Loss of oxide film would particularly affect heat transfer performance of the outer rods of a test bundle because they radiate to the hexagonal duct in the CFTL. Heat transfer analysts might well consider this possibility.

Another spare rod for THORS bundle 4A having no internal thermocouples was operated in the 2-atm (15-psig) helium environment to obtain

resistivity data for boron nitride (BN) at high temperatures. The rod was coated with zirconium oxide to provide uniform emissivity and installed such that the dc-leakage current through the Bi¹ to the heater sheath could be measured. The temperature of the rod surface was measured with two each type S intrinsic thermocouples made of 0.25-mm bare wire and also with an optical pyrometer. Temperature readings of the thermocouples and pyrometer were in good agreement up to 1150°C, but at 1200°C and above the dc-current leakage to the bare wire intrinsic thermocouples created large downscale errors. Only the pyrometer was used for temperature measurements above 1150°C. The leakage to the bare wire thermocouples demonstrated the need for sheathed thermocouples with insulated junctions in the CFTL rods.

A brief test was run to determine if natural convection currents of helium along the 7.5-mm-OD by 1130-mm-long heated length of rod caused temperature measurement errors in the bare wire, 0.25-mm-OD, type S, intrinsic thermocouples attached to the vertically suspended rod within the test vessel. Thermocouple and pyrometer readings agreed both in helium at 2 atm and in vacuum at 1100°C. This demonstrated that no significant errors ($>\pm 5^\circ\text{C}$) were caused by helium convection currents within the test vessel.

The next CFTL prototypic rod (GLS-L-4P) tested was fabricated with a platinum-tungsten heating element of uniform heat generation with molybdenum central and annular conductors and stainless-steel-sheathed, type K, MgO-insulated, internal thermocouples. The rod was coated with ZrO_2 to ensure stable high-temperature operation in the 2-atm helium test environment. No dc-leakage effects on internal thermocouples were found during a series of 16 tests over a period of 9 h at temperatures of up to 1200°C. Figure 1.6 shows typical thermocouple signals recorded on the DAS during a power interruption of about 3.5 s. There was no instantaneous change in any thermocouple signal as the power was turned off and on, which demonstrated that there was no significant dc leakage at this test condition. Test results during a potential reversal while operating at a temperature of 1088°C are shown in Fig. 1.7, and again no dc leakage is evident on the two thermocouples that were still operating

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Fig. 1.6. Thermocouple Output vs Time During Voltage Transients at $\sim 1060^{\circ}\text{C}$.

Fig. 1.7. Effect of Potential Reversal on Thermocouple Signals at $\sim 1088^{\circ}\text{C}$. Induced noise occurs during switching, but no dc leakage is evident.

at the time of this particular test. The first signs of dc-leakage effects occurred at a test temperature of 1230°C (2250°F) as shown in Fig. 1.8. A maximum error of $<10^{\circ}\text{C}$ was observed on one of two internal thermocouples during potential reversal at 1230°C. Therefore, the error during operation would have been about one-half that observed during potential reversal or about 5°C . The rod temperature was increased to 1260°C, but no further data were obtained before rod failure occurred due to the extreme operating temperature. Severe drift and decalibration effects were observed on the type K thermocouples at these extreme operating temperatures prior to rod failure.

Posttest examination of rod GLS-L-4P showed that the rod suffered mechanical failure during cooling, which followed operation at 1260°C. There is substantial differential thermal expansion during high-temperature operation between the type 316 stainless steel cladding and the internal molybdenum central conductor. The differential growth between the cladding and the central conductor along the 1130-mm-long (44.5-in.) heated section was about 19 mm (3/4 in.) at the maximum test temperature of 1260°C (2300°F). This large differential expansion appears to have been accommodated during initial operation of the rod by failure of a

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Fig. 1.8. Potential Reversal Showing First Evidence of dc Leakage at an Operating Temperature of 1230°C.

weld joining the molybdenum conductor rod to the bore of a platinum adapter at the lower (closed) end of the rod. After the failure, the molybdenum conductor rod could slip in the adapter, permitting it to move somewhat independently of the other rod components during operation with increasing rod power. However, during the subsequent cooldown from approximately 1260°C after rod power was shut off, the rod and adapter apparently locked, and the differential movement strained the cladding and the boron nitride sufficiently in tension to break them, normal to the rod. The cooling process put the molybdenum rod into compression. The rod sheath, heating element, internal thermocouples, and boron nitride insulation were separated in tension at two different rod axial locations because the stainless steel cladding contracted much more than the molybdenum conductor rod. Only the central molybdenum conductor (2.17 mm OD) was intact after the rod cooled to room temperature.

The test showed that the present "advanced" rod design is unable to withstand cycling and the resultant differential thermal expansion associated with severe thermal cycling. The heavily compacted boron nitride insulation apparently couples mechanically and very strongly the stainless steel cladding to the molybdenum conductor, despite their disparate coefficients of thermal expansion. The mechanical failure of advanced rod GLS-L-4P was a major factor in the decision to concentrate efforts on the reference design rod. The reference design rod cannot provide clad melting at high levels of rod power because of temperature limitations on both the nickel central conductor and the Nichrome V heating element. However, the reference rod should have the capabilities to meet the requirements of the majority of the tests outlined in the GA program plan and the GA test specifications, excepting a portion of the design margin undercooling tests. Differential growth at high temperature between the nickel central conductor and the type 316 stainless steel sheath in the reference rod is about one-fourth that of the advanced rod. A reference design rod, with stainless-steel-clad thermocouples, was operated in the initial test of this series and successfully withstood 15 cooling cycles from temperatures of 700 to 1000°C. Thus there is experimental evidence suggesting that the reference design can accommodate internal differential expansion. However, further tests will be

made to demonstrate adequate performance of reference rods for the CFTL bundle test program.

Test rod GLR-L-6P had a Nichrome V heating element of uniform heat generation and contained three 0.6-mm-OD (0.020-in.), Inconel 600-sheathed, type K, MgO-insulated internal thermocouples with insulated junctions. This was the first CFTL prototypic rod to operate with Inconel 600-sheathed thermocouples, which were selected because of reduced decalibration observed in the CFTL thermocouple development program reported elsewhere. The rod was nonstandard because its central conductor had broken internally during final swaging operations. The damaged rod was salvaged by opening the lower end of the cladding to permit attachment of a conductor to complete the electrical circuit across the heating element. Because of this modification, no current passed through the central conductor during test operation. The test was run in this manner because the central conductor could not be repaired, and the modification did not affect dc-leakage measurements.

A vacuum was maintained in the test vessel, which contained the rod during operation. Therefore, the rod was cooled only by radiation to the air-cooled walls of the test vessel. Vacuum was used in this test to preclude helium ionization at the nonstandard oper. end of the rod within the test vessel.

The rod was tested to observe dc leakage to the internal thermocouples at temperatures of 600, 700, 800, and 900°C, and no leakage was found. At this point, a short circuit in the wiring system imposed a large increase in potential to the rod heating element, which caused a rapid rod temperature rise to 1250°C (2280°F) before the power was turned off manually. Inspection of the rod showed no obvious damage; the short circuit was therefore repaired, and operation was resumed. Tests were repeated at temperatures from 600 to 1100°C with no evidence of dc-leakage errors on thermocouple outputs. The rod failed as the temperature was being increased to 1150°C (2100°F), and the test was terminated. The rapid thermal cycling caused by the short circuit might have hastened the rod failure.

This data confirmed previous rod testing, which showed no measurable dc-leakage effects on internal thermocouples at operating temperatures

up to 1100°C. Data were also taken on total current leakage to the rod sheath at each temperature level and at both normal and reversed potential to characterize the electrical resistivity of the compacted BN insulation within the rod. X-rays and resistance measurements, taken before and after operation, showed that the internal thermocouples survived the severe thermal cycling of this test. This demonstration was the first to show that differential expansion between the Inconel-sheathed thermocouples and stainless-steel-rod cladding could be accommodated over a large operating temperature range.

Another reference design fuel rod simulator (GLR-I-7P) with uniform heat generation, ZrO₂ coating, and Inconel 600-sheathed internal thermocouples has now operated about 800 h at nominal CFTL operating temperatures in a 2-atm helium environment. We plan to test this rod primarily to gain additional information on high-temperature BN electrical resistivity and to observe rod performance during both prolonged periods of operation and thermal cycling. Additional testing will be required in the sodium test facility to demonstrate rod performance during many thermal cycles while operating at high heat fluxes.

Measurements were taken during operation of test rod GLR-I-7P to determine if vertical motion of the central or annular conductors occurred as a result of differential thermal expansion within the rod. No vertical motion of either conductor was observed at operating temperatures of 600, 700, 800, and 900°C during 800 h of operation. Therefore, differential growth within this reference design rod is thus far being accommodated within the cladding.

No further dc-leakage tests are planned because of the recent decision to use ac power for the CFTL test bundles.

1.2.3 Electrical Power and Control System for CFTL Test Bundles - T. L. Hudson

1.2.3.1 Evaluation of Candidate ac- and dc-Power Supply and Control Systems

During this report period, evaluation of candidate ac- and dc-power supplies continued (see p. 18 of ref. 4). Both ac and dc control

systems use silicon controlled rectifiers (SCRs). The ac-power supply uses zero-voltage-firing SCRs, and the dc-power supply uses phase-angle firing.

Based on the relative evaluation of the candidate power supplies and controls for CFTL test bundles presented in Table 1.2, the linear multipower level (LMPL) ac-power supply has been selected for the CFTL test bundle.

The purchase and installation costs, availability, and delivery times of the equipment are about the same for both systems. The two systems also have about equivalent capabilities to meet the requirements for supplying and controlling power to the CFTL test bundles. However, they differ in areas of electromagnetic interference (emi) noise and current leakage into thermocouples within the test bundle rods.

There is much less noise associated with the zero-angle firing mode of SCR operation for the LMPL system than with the phase-angle firing mode used in the dc-rectified system. The amount of current leakage from the heating element onto the internal thermocouples is a function more of rod characteristics than of power-supply characteristics.

Table 1.2. Relative Evaluation of Candidate Power Supplies and Controls for CFTL Test Bundles

Characteristic	ac System	dc System
Equipment cost	~ Same	~ Same
Equipment availability	Acceptable	Acceptable
Equipment delivery	Acceptable	Acceptable
Equipment installation cost	~ Same	~ Same
Steady-state operating range	Acceptable	Acceptable
Transient operating ranges	Acceptable	Acceptable
Spurious rod motion during steady-state operation	Acceptable	Acceptable
Spurious rod motion during transient operation	Acceptable	Acceptable
Emi noise radiated, effect on thermocouples	Acceptable	Acceptable
Emi noise radiated, effect on neighboring equipment	Acceptable	Unknown
Emi noise conducted, effect on thermocouples	Acceptable	Acceptable
Emi noise conducted, effect on neighboring equipment	Acceptable	Unknown
Current leakage onto thermocouples, up to 1200°C (2200°F)	Acceptable	Acceptable
Current leakage onto thermocouples, above 1200°C (2200°F)	Filterable	Unknown
Program control accuracy, high range	Acceptable	Acceptable
Program control accuracy, low range	Acceptable	Acceptable
Program control	Feasible	Feasible
Power measurement	More feasible	Feasible

However, the LMPL ac system permits near elimination of the deleterious effects of current leakage on rod temperature measurement by the application of a carefully designed filter, which cannot be said for the dc-rectified system. Thus the ac system was chosen because of reduced leakage current effects and superiority with respect to the deleterious effects of radiated and conducted emi noise.

1.2.3.2 Linear Multipower-Level ac-Power Supply and Control Systems

The schematic diagram for the LMPL supply (500 kW) for an individual CFTL test bundle zone is shown in Fig. 1.9. The system consists of an induction regulator, a multitap transformer, and 15 SCRs, which provide 15 (120-Hz) power steps between 100 and 0% power. The induction regulator

Fig. 1.9. Schematic Diagram of Linear Multipower-Level Zone ac-Power Supply.

in the transformer primary is planned to be either manually operated or motor driven. When SCR pair 15 is turned on (maximum voltage), the induction regulator provides continuous adjustment to about 0.5% of rod-rated power. Both manual and computer control will be available for SCR firing. Transients as slow as 30 min and as fast as about 1 s are controlled.

1.2.3.2.1 Steady-State Operation. The setting of all steady power levels will employ the induction regulator. Typically only SCR pair 15 will be turned on for steady-state operation. If steady-state operation below 2% of rated rod power is needed, a lower-power-level SCR pair will be turned on.

1.2.3.2.2 Transient Operation. All transients will be generated, based on normalized 100 to 0% curves. The 100% level will be set up by varying the induction regulator with SCR pair 15 turned on. Thus all zone steady-state operation for both uniform and skewed power distributions will be obtained with the zone induction regulator. When the 100% level has been attained, a minicomputer will generate the transient.

Both open-loop and closed-loop control of the SCRs are being considered for transient operations. In the open-loop control, the SCRs would be stepped from SCR pair 15 to SCR pair 1 after the induction regulators were adjusted to obtain the required steady-state (initial) operation. The voltage at each level would be recorded and used in programming the transients to compensate for the variation in transformer secondary winding-tap voltages that cause departure from linear power steps and for the voltage regulation of the zone power supply during load changes. The closed-loop control system would minimize the effects of variation in transformer secondary windings and the lack of voltage regulation during load changes.

1.2.3.2.3 Spurious Rod Motion. The spurious rod motion associated with the operation of the LMPL system has been studied and reported (see pp. 11-18, ref. 4). The planned use of the induction regulator during steady-state operation gives rise to a dominant power frequency of 120 Hz, which produces nil spurious rod motion. The use of a programmable motor controller is being evaluated for the induction regulator drive

motor to vary the output of the induction regulator as needed for slow transients. This would also yield a dominant power frequency of 120 Hz. The operation of the SCRs to supply fast power transients should produce no deleterious rod motion.

1.2.3.3 Evaluation of Zero-Fired SCR dc-Power Supplies

The main objective in evaluating zero-fired SCR dc-power supplies was to combine as many of the desirable features of the dc phase-fired power supply and the ac zero-fired power supply as feasible. The following four systems were studied:

1. six-level zero-fired dc-power supply (3 ϕ primary),
2. twelve-level zero-fired dc-power supply (3 ϕ primary),
3. fifteen-level zero-fired dc-power supply (1 ϕ primary), and
4. fifteen-level zero-fired dc-power supply (3 ϕ primary).

The 6-level and 12-level zero-fired dc-power supplies are modifications of the dc phase-fired power supply and are unsuitable for the CFTL power supply because of the nonlinear power steps, which vary from 1 to 17% of full power.

The 15-level zero-fired dc-power supply (1 ϕ primary) is a modification of the 15-level LMPL ac-power supply. The secondary of the zone transformer is changed from a single winding with 15 taps to 15 separate windings. The arrangement of the SCRs and the blocking diodes permits any combination of the 15 windings to be connected in series. The effective heating-wave shape for this power supply is the same as the LMPL ac-power supply. Thus the estimated spurious-rod motion for both steady-state and transient operation would be the same as the LMPL ac-power supply.

If the 15-level zero-fired dc-power-supply (primary) winding is changed from single phase to three phase, the only improvement is in the reduction of the spurious-rod motion during steady-state operation. Thus the zero-fired dc-power supply does not have a major advantage over the LMPL ac-power supply.

1.2.3.4 Power Measurement (with C. F. Holloway)

An ac-power transducer and current and potential transformers were calibrated both individually and as an ac-power-measurement system.

Their accuracy was established during operation in the 0.5 to 100% power range for the CFTL fuel rod simulators.

The evaluation of the calibration data indicates that accurate steady-state data over the power range can be obtained using one size for both the instrument transformer and the watt transducer. The current transformer ratio error was less than 0.25% for primary current in the range of 5 to 200 A, and the potential transformer ratio error was less than 0.25% for primary voltage in the range of 10 to 360 V.

The accuracy of the systems calibrated was as follows:

- from 9 to 50 kW fuel rod simulator (FRS) power, $\pm 0.5\%$;
- from 3 to 9 kW FRS power, $\pm 1.0\%$;
- from 1 to 3 kW FRS power, $\pm 2.0\%$;
- from 0.6 to 1 kW FRS power, $\pm 3.0\%$; and
- from 0.2 to 0.6 kW FRS power, $\pm 6.0\%$.

This data supplied to a computer for steady-state power calculation can reduce the error to $< \pm 0.5\%$. To obtain this accuracy, the components should be calibrated as individual systems before being field installed in the same manner.

This ac-power-measurement system will be installed at the Single-Rod Test Loop to determine the fast transient capabilities.

1.2.3.5 Bundle Rod Electrical Protection

Electrical protection to be provided for the bundle rods includes (1) current-limiting fuses, (2) overcurrent protection, (3) leakage-current protection, and (4) ground-current protection. The primary function of this protection is to enhance the reliable operation of CFTL test bundles by reducing or preventing damage to the rods by electrical faults.

A schematic diagram of the CFTL rod electrical protection is shown in Fig. 1.10. The overcurrent sensor monitors the current in the high potential lead to the rod. Overcurrent can be caused by electrical shorting between the heating element and the center rod or between the heating element and the sheath. Whenever the current exceeds the preset level of the detector, the detector provides a signal to turn on the crowbar SCR. The crowbar SCR then applies a short circuit across the

The leakage-current sensor monitors the difference in the current in the two leads to the rod, which could be caused by leakage of current through the BN insulation from the heating element to the sheath or by any other electrical shorting of current to the sheath. When the current exceeds the present level of the detector, the crowbar SCR is turned on, and the action is the same as described for the overcurrent protection.

The ground-current resistor limits the maximum ground current of the power-supply system, including the test bundle. The ground-current detector circuit will be used to monitor the ground current and to provide control action.

Field tests will be made after the protection system is installed to determine the minimum setting of the current monitors. It is currently estimated that the minimum settings will be about (1) overcurrent, 125 to 140% of rated rod current; (2) ground current, 20% of rated rod current; and (3) leakage current, 5% of rated rod current.

The preliminary design of a fault detector for both overcurrent and leakage current which would be usable with either ac- or dc-power systems has been completed, and the modules are being tested and evaluated with both ac- and dc-current sensors. After the initial tests, required modification of the test modules will be made, and final proof testing will be made in the Single-Rod Sodium Loop (SRSL) during operation with the fuel rod simulators.

1.2.3.6 Bidirectional Current Sensors

Two bidirectional current sensors, manufactured by American Aerospace Control, Inc., as model 910 and having a 200-A range, were tested. These devices sense either ac current or dc current whose polarity or direction of flow may reverse. They act as input sensors for both overcurrent and leakage-current protection. The sensors produce a dc-signal output that is electrically isolated from the line current.

The current sensors were operated with a dummy load connected to the 60-kW dc-power supply. The tests of leakage-current measurements indicated that the use of a separate current sensor in each fuel rod simulator (rod) lead is not a practical method for operation over a wide current range because the nonlinear output of the two preamplifiers in the fault detector would require resetting the unit for each current level.

Tests were made to simulate leakage-current measurements, using a single-current sensor in which both of the rod leads pass through the opening in the sensor. The current-sensor output signal was proportional to the simulated leakage current and was independent of the rod

current level. The 3/8-in. opening in the current sensor limited these tests to rod current of about 75 A. Other current sensors with a larger opening will be selected for testing.

1.2.3.7 Alternating Current Sensors

Tests with an ac sensor, model GA-3737 manufactured by Square D Company, were made in a test setup, using a simulated rod operated over the range of 10 to 150 A with a leakage-current range of 50 mA to 8 A. The sensor output signal, which is electrically isolated from the line current, was proportional to the simulated leakage current and was independent of the simulated rod current level. In addition, the output signal was proportional to the load resistor across the output terminal for the 1- to 100- Ω range of resistance tested. The output signal of about 200 mV/A with the 100- Ω load resistor compares to an output signal of 2.5 mV/A obtained with a bidirectional current sensor described in the preceding section. Because there should be fewer problems with the larger output signal, we believe the ac sensor will be a superior device for ac-leakage-current detection.

Several tests were made with the ac sensor and a current-detector module over a wide range of leakage current. In general, the minimum leakage current that could be detected was 10% of the maximum setting.

A Westinghouse style 179C768H01 current sensor was tested with a simulated rod operated at currents up to 150 A and a leakage current ranging from 50 mA to 8 A. The output signal was about 250 mV/A with a 100- Ω load resistor. Either this current sensor or the Square D model GA3757 provides adequate signal for the leakage-current detectors.

1.2.3.8 Fifty-kilowatt Power Supply for Single-Rod Tests

The design and installation of a 50-kW ac-power supply of the LMPL scheme was completed for the SRSL. The power-supply output is continuously adjustable from about 0.5 to 100% of rated rod power and can be operated as a ten-level power supply up to 300 V and 90 A or a five-level power supply up to 160 A. Several prototypes of the CFTL fuel rod simulators (rods) will be installed in the SRSL and will be

operated with the ac-power supply. The power supply will be evaluated while the CFTL rods are simultaneously proof tested.

1 2.4 Fuel Rod Simulators for CFTL Test Bundles — R. W. McCulloch

1.2.4.1 Introduction

This report delineates the progress toward development of a reliable, accurate, high-temperature, high-heat flux fuel rod simulator (referred to as FRS or rod) for use in the CFTL. The rod shown in Fig. 1.11 is intended to simulate thermally and mechanically the fuel rods in GCFR core assemblies during programmed testing in the CFTL.

The reference CFTL rod is reentrant in design, with both electrode terminals entering the rod at a single end. A copper-to-nickel tubular terminal is joined to a variable-width Nichrome-V-ribbon heating element designed to provide a cosine heat flux distribution having a maximum-to-average heat flux of 1.21 to 1. The other terminal copper current return is joined to a nickel center rod. Total rod length is 2260 mm (89 in.) with a heated length of 1130 mm (44.5 in.). Cold pressed preforms of boron nitride (BN) provide thermal conduction and electrical insulation between the heating element, with its associated tubular lead-in, and the nickel center electrode and the rod sheath or cladding.

The rod will be designed to operate at power levels up to 45 kW, which provides a heat flux of 1.7 MW/m^2 ($540,000 \text{ Btu h}^{-1} \text{ ft}^{-2}$), with a maximum sheath temperature of 1000°C (1832°F) at a maximum heating-element temperature at 45 kW of 1250°C (2282°F). Higher clad temperatures can be obtained with reduced values of rod power.

These operating conditions place stringent requirements on all materials used in the rod. The heating element must operate at the maximum temperature without property degradation; the BN must maintain both high thermal conductivity and electrical resistivity at the maximum temperatures; in addition, its properties must remain uniform axially and circumferentially to maintain accurate simulation of the cosine heat flux. The rod must also maintain its integrity while enduring thermal stresses generated during rapid CFTL thermal transient tests.

Fig. 1.11. CCFR Core Flow Test Loop (CFTL) Fuel Rod Simulator.

The FRS development group at ORNL performed materials and fabrication development, inspection, testing, and failure analysis studies to support the industrial procurement of these state-of-the-art rods for the CFTL.

1.2.4.2 Fuel Rod Simulator Prototype Fabrication

A total of seven fuel rod simulator prototypes were fabricated locally in the continuing effort to develop high-performance rods in support of the CFTL. Three were successfully fabricated, two were partially successful, and two failed during fabrication. Four underwent operational testing in a dc-leakage test (see Sect. 1.2.2). Several major problems were encountered, and a great deal was learned. Emphasis during the earlier part of the year was on the high-temperature advanced rod design using noble alloy materials. The emphasis shifted later in the year when efforts were focused on the reference rod design that features nickel conductors and a Nichrome V heating element. Much information gained on noble rod fabrication was applicable to reference rods; this section reports this development. Development of associated technology such as preform development, variable-width-ribbon heating element fabrication, etc., are reported in subsequent sections.

Table 1.3 summarizes fabrication efforts for the past year. The first fabrication attempt was aborted because of problems with terminal-to-preform sizing tolerance. However, information and tooling developed for it were used to fabricate the first instrumented CFTL rod, GLR-L-1P. This rod contained a Nichrome V heating element, a nickel tubular terminal, and a molybdenum central rod and current return. Four type K stainless-steel-sheathed thermocouples (T/C), two insulated junctions (IJ), and two grounded junctions (GJ) were welded to precise axial and circumferential locations inside the sheath. The unit was tested in the dc-leakage test and failed shortly after reaching clad melting.

The next three units (GLS-L-2P, -3P, and -4P) contained a platinum-8% tungsten alloy heating element and molybdenum tubular and central terminals. Only the last of the three was successfully fabricated. The first failed when a molybdenum-platinum laser weld separated during swaging, and the second failed during the postanneal infrared scan due

Table 1.3. CFTL Fuel Rod Simulator Fabrication Summary

Rod serial no.	Heating element material	Tubular lead materials	Central rod materials	Instrumentation ^a	Tested	Remarks
None	Nichrome V	Nickel	Molybdenum	None	No	Sizing tolerance too large; fabrication discontinued
GLR-L-1P	Nichrome V	Nickel	Molybdenum	4 0.5-mm type K T/C with stainless steel sheath; 2 IJ; 2 GJ	Yes	First GCFR FRS operated satisfactorily in de-leakage test
GLS-L-2P	PT-8% W	Molybdenum	Molybdenum	4 0.5-mm type K T/C with stainless steel sheath; 2 IJ; 2 GJ	No	Pt-W to Mo butt weld separated during sawing
GLS-L-3P	PT-8% W	Molybdenum	Molybdenum	4 0.5-mm type K T/C with stainless steel sheath; 2 IJ; 2 GJ	No	Failed during post-anneal infrared scan; organic impurity in central BN
GLS-L-4P (GCR-Y4)	PT-8% W	Molybdenum	Molybdenum	4 0.5-mm type K T/C with stainless steel sheath; 4 IJ	Yes	Successful; operated up to 1250°C; severe differential thermal expansion mismatch
GLR-L-5P	Nichrome V	Nickel/copper	Nickel/copper	4 0.5-mm type K T/C with Inconel 600 sheath; 4 IJ	No	Successful; two Inconel T/Cs broke at tab during fabrication
GLF-L-6P (FR-6P)	Nichrome V	Nickel/copper	Nickel/copper	4 0.5-mm type K T/C with Inconel 600 sheath; 4 IJ	Yes	Lost 1 T/C; Ni-Cu braze failed; repaired and operated to 1250°C
GLR-L-7P	Nichrome V	Nickel	Nickel	4 0.5-mm type K T/C with Inconel 600 sheath; 4 IJ	Yes	Successful; in operation

^aT/C = thermocouple; IJ = insulated junction; and GJ = grounded junction.

to internal organic contamination. Improvements in terminal joining techniques using these materials were developed as a result of the first failure. Modification of the fabrication procedure to allow a check of the active component assembly for organic impurities was incorporated as a result of the second failure.

Unit GLS-L-4P was tested in the dc-leakage test and was subjected to rigorous operation. Boron nitride (BN) preform resistance vs temperature data up to 1200°C was obtained for the annular region. Data showed the BN resistance to be 3 k Ω at 1200°C (2200°F). The high-temperature resistance slope of approximately one decade decrease per 150°C indicated that the resistance remained adequate up to clad melting temperatures. Rod failure occurred at a measured sheath temperature of 1260°C (2300°F) and 8 kW of power, but no BN resistance data were obtained at that temperature.

Failure analysis of the prototype indicated that excessive differential thermal expansion between the central molybdenum rod and the 316 stainless steel sheath contributed to its failure. The lower thermal expansion of molybdenum [6.1×10^{-6} m/(m·k) vs 19.8×10^{-6} m/(m·k) for austenitic stainless steel at 1200°C] resulted in shearing the weld that joins the end plug to the molybdenum rod. The molybdenum rod moved about 1.75 cm with respect to the plug. Normal operation was experienced at the high temperatures; however, when the rod cooled, the molybdenum rod became hung up rather than sliding freely into the end plug. Tensile forces in the stainless steel sheath, caused by its greater contraction during cooling than experienced by the molybdenum rod, resulted in the sheath's separation at two axial locations totaling about 1.75 cm.

Because of the differential thermal expansion problems experienced on GLS-L-4P as well as the withdrawal of funding support for platinum-tungsten alloy improvement, emphasis was shifted to the reference rod design of the Nichrome V heating element and the copper-nickel terminals on subsequently fabricated rods. In addition, the decision was made to substitute Inconel 600 material for the stainless steel sheath on the type K thermocouples whenever the Inconel sheaths exhibited less decalibration errors at temperatures up to 1150°C.

Rod GLR-L-5P was fabricated using reference materials. During fabrication, tabs welded to the junction ends of two of the four internal

thermocouples broke off. Otherwise fabrication of the unit was normal. The rod was not tested because insufficient data would have been obtained without complete internal instrumentation.

In the next rod, GLR-L-6P, one of the four Inconel tabs broke from a thermocouple sheath during rod fabrication. The tab fabrication procedure, which consisted of welding a bead on the junction end and swaging it down to the T/C diameter, was reviewed and found to be adequate. However, an annealing step was included subsequent to tab fabrication, and it was decided that the 1.36-kg (3-lb) tensile force, applied to each T/C during rod fabrication, should be reduced to a value of 0.23 kg (0.5 lb).

Separation of the nickel-copper central rod braze joint occurred during swaging of rod GLR-L-6P. Failure occurred because of inadequate temperature control of the braze joint during brazing. The brazing operation for the rod is being modified (see Sect. 1.2.4.7).

The rod was repaired and operated in the dc-leakage test. It failed at a measured sheath temperature of 1150°C (2100°F). Analysis indicated that the failure occurred because there was a low-density area in the inner BN insulation. Several hours prior to failure of the rod during operation at a measured temperature of 1150°C, a short had occurred in the electrical circuits external to the rod, causing a sharp temperature ramp that brought the rod cladding temperature up to 1250°C. Though the rod survived this incident, the subsequent failure might have been initiated at that time due to overheating in the area of low-density BN, which, of course, had lowered thermal conductivity.

Rod GLR-L-7P was successfully fabricated with four Inconel T/Cs intact. The variation in the infrared scan made during transient heating of the rod was just within the $\pm 5\%$ maximum tolerance. Concentricity was excellent [no eccentricity was measurable throughout the 114-cm (45-in.) heated length], final BN density was about 90% of theoretical and very uniform, and all electrical checks were excellent. Because the variation during transient heating was expected to be more uniform than $\pm 5\%$, additional investigations into the cause of the variation were made. It was found that 7 to 10% variation in the sheath wall thickness existed. When the effects of these wall-thickness variations were subtracted from variations in the infrared scans made during core and clad transients, the profile was markedly improved.

Clad thickness variations were traced to defects in a portion of the tubes, which had been centerless ground to reduce the wall thickness. Defective tubes were discarded, and a new lot of tubing was ordered. The rod is currently undergoing operation in the dc-leakage test at clad temperatures from 700 to 900°C.

Analysis of fabrication data to date indicates that several minor changes are needed to improve uniformity of the reference rod. These are to be incorporated into subsequently fabricated rods. Terminal joining is still a problem, but improvements in progress should resolve it.

A great deal of fabrication information has been obtained and is being incorporated into an evolving fabrication procedure. Most procedures are understood and under control. The SRSI has been modified to accept CFTL rods and to subject them to operational tests to establish rod performance at high power and medium temperature (600 to 1000°C) and during thermal transients. These tests and further evaluation of the fabrication process, if needed, will be completed during the coming year.

1.2.4.3 Elimination of Metallic Particles from Boron Nitride Powder

Two types of BN powder are available for use in CFTL fuel rod simulator fabrication. These are HCM, a high-purity hot pressed and machined powder with a well-defined particle size, and TS-1325, a high-purity powder with a large variation in particle size and a high percentage of fines. Until recently, TS-1325 was the primary BN preform powder. However, HCM is now used more since a die-body lubrication system for pressing preforms has been introduced.

Both powder types contained about 0.5 metallic particles per cm³ when received from Union Carbide Corporation (UCC), Parma, Ohio. Both powders were within UCC specifications of <500 ppm of metallic impurities; however, both were considered unacceptable inasmuch as these particles can act to reduce substantially the electrical resistance in a finished preform. About 340 kg (750 lb) of the UCC BN powder was magnetically cleaned by Magnetic Engineering Associates (MEA), Cambridge, Massachusetts. Particle count, as measured by radiography, was reduced to about 0.1

particle per cm^3 . Conductivity test cells, fabricated and tested at ORNL, indicated that this value was acceptable for the HCM powder. However, the TS-1325 powder still contained some large (>0.5 mm) particles. Additional powder cleaning by sieving is being accomplished at ORNL before use.

To alleviate the long-term problem of particulate impurities in high-purity BN powder, a contract was placed with UCC, Parma, Ohio, to obtain 69 to 91 kg (150 to 200 lb) of improved BN powder similar to both HCM- and TS-1325-type powders. This contract will provide the basis for preparation of a specification on which future powders will be purchased. Development of improved impurity-measuring techniques by the Y-12 Development Division is being coordinated with this effort. The goal is to obtain a specification (and supplier) to produce BN powder that meets all rod high-temperature insulation requirements.

1.2.4.4 Boron Nitride Preform Development — C. S. Morgan

The fabrication of pressed BN preforms for use in high-performance fuel rod simulators was extended from the laboratory to commercial production during the year. Eagle-Picher Industries (EPI), Miami, Oklahoma, was chosen as the commercial vendor. More than 356 m (14,000 in.) of preforms was produced for Breeder Reactor Program (BRP) rods.

Automatic production proved to be very difficult, and several major changes in powder pretreatment and pressing were required to make it successful. Although a sophisticated electronic hydraulic press was purchased by Eagle-Picher for BN preform pressing, the die-body lubrication system could not adequately provide the lubrication, drying, and cleaning needed to fully automate the process. ORNL assisted by initiating a development effort to determine a lubricating agent that would permit pressing while eliminating the need for die-body lubrication and cleaning. After several months of investigations, a lubricating additive consisting of 0.5% dodecane and 0.5% dodecal alcohol was found to be partly successful. The pressing of 50 to 100 preforms was possible before die cleaning was necessary. Preform densities of 1.9 to 2.0 g/cm^3 were achieved, and preform strength was excellent. These results were

obtained only when the powder was prepressed at 82.7 MPa (12 kpsi), broken up, and screened through 20 mesh on 100-mesh sieves. Many other powders were tried, but most would not provide adequate lubrication, had no "green" (prebaked) strength, or lost too much density and strength during purification. Purification involves the removal of lubrication additive impurities during bakeout at high temperature.

It was found that preform density decreased to 1.6 to 1.7 g/cm³ when purified and that radial dimensions increased approximately 2%; postpurified strength was still acceptable. It was subsequently determined that preform density increased from 1.6 to 1.7 g/cm³ to 1.78 to 1.8 g/cm³ when the pellets were crushed in place during FRS fabrication, making them capable of yielding the high final density required in an FRS with minimum swaging.

The original goal of the BN preform industrialization effort was to attain an automatically produced preform with a final density of 1.85 g/cm³ or more. Toward this end, another die-body lubrication system (DBLS) was designed by Hydramet American Company, Royal Oak, Michigan (the press supplier), and purchased by ORNL for use on the EPI press. The new DBLS incorporated the valuable experience gained during the initial work with the original DBLS.

The DBLS was installed in June 1977 and was an immediate success. Preforms can now be pressed with a final density of more than 1.9 g/cm³. In addition, most preform sizes can now be pressed, using HCM powder containing no internal lubricating agent. Pressing rates are about the same as with lubricated powders, but the pretreatment process is eliminated for HCM powder.

A contract to develop appropriate automatic pressing techniques for the inner and outer annular preforms needed for the CFTL FRS is now in effect. Die sets have been procured by EPI, and development of the pressing procedure for the outer annular preform is proceeding normally. About 25 m (1000 in.) of each type of preform will be shipped to ORNL for evaluation and use when the development is complete. Production price estimates for quantities from 25 to 500 m (1000 to 20,000 in.) will also be included.

1.2.4.5 Variable-Width-Ribbon Heating Elements - R. T. Lowell and D. W. Post

Development of variable-width-ribbon heating elements for the CFTL FRS was done in parallel with the initial development of similar heating elements for FRSs in other programs, as summarized in the last progress report.⁴

Machining of the element to contour tolerances of 2.5 μ m (0.0001 in.) was accomplished by the Y-12 Fabrication Division with a template grinding machine, using an oscillating-rotating alumina grinding wheel. Initially a stack of 12 strips of Nichrome V material was machined to produce 12 variable-width flats to be wound into heating elements. After the process was proven to be accurate, it was improved to produce 45 flats from a stack of 15 strips of starting material.

Heating-element winding was accomplished using a numerically controlled, four-axis filament winder manufactured by Black-Clawson. After the initial developmental period, using the first 12 machined flats, little trouble was encountered, and all ribbons were wound successfully.

The turn-to-turn spacing between adjacent coils in a wound heating element is adjusted during a process in which the element is wound tightly on and fastened to a steel mandrel. Turns are allowed to touch near the center length of the coil, and an approximately 0.25-mm (0.010-in.) gap between turns is maintained at each end of the element where the material width is largest.

The entire assembly is then swaged to set the spacing pattern and coil diameter to conform to the required dimensions. This process compensates for variations in gaps that result when the coil is stretched during FRS fabrication.

Following swaging, the element is ready for assembly after it is removed from the mandrel, cut to the correct length, and cleaned. One hundred thirty-five completed, variable-width heating elements are now on hand for use in fabrication of the rods for the first CFTL test bundles.

1.2.4.6 Platinum-8% Tungsten-Alloy Heating Element - D. E. Harasyn and R. L. Heestand

Premature failure of prototype FRSs having platinum-tungsten heating elements prompted an investigation of the material properties. The

results of the investigation are:

1. Accelerated grain growth occurs in the alloy at temperatures of 1300°C (2300°F) and above.
2. Boron, freed from the BN, diffuses into the grain boundaries of the platinum-tungsten alloy and forms a boron-platinum eutectic, which lowers the alloy melting point to about 1300°C (2300°F).

Methods to retard grain growth, to protect the heating element from boron attack, and to eliminate the free boron were studied. Of these, the most successful was that of grain-growth retardation.

After several attempts, excellent grain-growth retardation was achieved with the homogeneous dispersion of 0.1% MgO into a powder of platinum-8% tungsten. The material, when processed by powder-metallurgy methods, exhibited stabilized grain growth both at high temperatures (up to 1600°C) and at medium temperatures (1200°C) for long periods of time. Figure 1.12 (a) and (b) show the modified and unmodified platinum-8% tungsten alloy respectively. The grain size of the modified alloy, after treatment at 1200°C (2192°F) for 27 h in vacuum, is essentially the same as it was at room temperature, while the control sample exhibits excessive grain growth.

Following this investigation, several feet of the modified alloy was prepared. Electrical resistivity, thermal expansion, and mechanical property tests were conducted. Except for a slight discontinuous increase in the thermal expansion above 1600°C and a small amount of uniformly distributed porosity, the modified alloy was equivalent to the unmodified one. Welding tests showed that, with slight modification of the procedure, welds were obtained which were equivalent to the unmodified sample.

Finally, a series of compatibility tests of the modified and unmodified alloys in intimate contact with the cleaned and purified BN were conducted. These results, illustrated in Figs. 1.13 through 1.16, showed that boron attack of the modified alloy was enhanced over that of the unmodified alloy, even though its grain size remained smaller. Apparently porosity present in the modified alloy contributed to increased diffusion rates and accelerated attack. In view of these data, it was concluded that operating time at high temperature (>1600°C) must be limited to a matter of minutes for rods fabricated with either alloy.

20 60 μm 140 180
0.001 400X in. 0.006

(a)

20 60 μm 140 180
0.001 400X in. 0.006

(b)

Fig. 1.12. Modified and Unmodified Platinum-Tungsten Alloy After Treatment at 1200°C (2192°F) for 97 h in Vacuum.

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Fig. 1.13. Platinum-8% Tungsten Alloy Imbedded in BN, Treated at 1200°C for 97 h.

Fig. 1.14. Platinum-8% Tungsten-0.1 MgO Alloy, Imbedded in BN, Treated at 1200°C for 97 h.

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Fig. 1.15. Platinum-8% Tungsten Alloy Imbedded in BN, Treated at 1500°C for 1 h.

Fig. 1.16. Platinum-8% Tungsten-0.1 MgO Alloy Imbedded in BN, Treated at 1500°C for 1 h.

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Concurrent with the above-mentioned tests, conductivity test cells made with each of the two alloys were fabricated locally. Identical series of tests were conducted on each in the SRS, culminating in operation at 1000°C and at 702 W/cm with the first test cell, while the second operated at an even higher power of 820 W/cm (320 W/cm² or 1.03×10^6 Btu h⁻¹ ft⁻²). It is notable that the second unit operated at a heat flux almost twofold higher than the maximum power level of CFTL rods. The central heating element reached a maximum temperature of 1354°C in the second test cell. These results suggest that grain-growth retardation is at least as important as protection from boron attack and that the modified alloy may be somewhat of an improvement over the unmodified version.

Funding for the alloy modification, primarily under Breeder Reactor Program Technology, was discontinued in FY 1978, and no GCFR funding was provided. Therefore, further tests on this alloy have been dropped, and current plans for the CFTL are focused solely on the reference design rod with a Nichrome V heating element.

1.2.4.7 Terminal Joining - A. J. Moorhead

Referring to Fig. 1.11, it is apparent that several joints between dissimilar materials must be accomplished. There are three terminal connections (copper to nickel rod and tube and nickel to Nichrome V tube) to be made, and each must be capable of carrying 150 A, must maintain integrity at 1000°C or higher, and must withstand mechanical stresses induced by swaging. The tubular terminals and the heating element have a wall thickness of 0.25 mm (0.010 in.), which makes the joining operation quite difficult. In addition, the tolerance on ID and OD of the tubular parts is ± 0.025 mm (0.001 in.).

The heating-element-to-ground-plug and ground-plug-to-central-rod connections also have to withstand high temperatures, mechanical stresses, and high current; but their connection is less difficult because of the configuration and larger total mass of the weld area of these parts compared with the tubular parts.

The Welding and Brazing Development Group of the Metals and Ceramics Division has performed most of the needed development. Emphasis was

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placed initially on the advanced rod with its platinum-tungsten heating element and molybdenum and copper conductors because of the need for a rod to conduct high-temperature dc-leakage tests for the thermocouple error evaluation (Sect. 1.2.2) and because of the more difficult task of joining these materials.

The optimum method for joining the molybdenum-copper tubular and rod connections was determined to be brazing with a high-temperature nickel-phosphorus braze. Because of the small diameter, thin wall, and the large difference in material melting points ($>1300^{\circ}\text{C}$), the joining technique was exacting. However, several successful connections were obtained before these materials were dropped in favor of nickel-copper for the reference rod design.

A laser welding technique was used to join the platinum-tungsten heating element to the molybdenum tubular terminal. Figure 1.17 shows a sample encased in the inert-gas containment vessel and a completed sample. To avoid molybdenum-platinum intermetallic compounds, the solution of molybdenum in platinum had to be limited to about 10%. This was accomplished by careful control of the laser energy. To further stabilize the connection, the molybdenum tube was beveled; after careful alignment with the heating element, a small-diameter platinum wire was tack-welded into the groove between the two materials. During the welding process, this filler material fused with both materials, forming a strong mechanical connection with little reduction of total cross-sectional area.

Techniques developed for the high-melting-point materials were extended to the nickel-copper and nickel-Nichrome connections for the reference rod. This worked well for the nickel-Nichrome weld, but failure of rod prototype GLR-L-GP indicated a need for additional braze connection development for reference materials. This development is in progress. Thus far, it has been determined that a nickel-gold-copper brazing alloy, with a 1100°C remelt temperature, results in a stronger nickel-copper connection. Pull tests conducted on nickel-copper rod joints made with this brazing alloy indicated about 138 to 172 MPa (20 to 25 kpsi) tensile stress before failure. This is adequate for fabrication but is only about 60% of the expected yield stress of the connection. Analysis of the fracture surfaces indicates incomplete flow of

Fig. 1.17. Laser Weld of Platinum-Tungsten Heating Element to Molybdenum Tubular Terminal.

the brazing alloy. Modification of the surfaces is being accomplished and is expected to increase further the connection strength and reliability.

To obtain better braze temperature control, the induction brazing process used to braze the nickel-copper rods is being replaced with a furnace vacuum brazing process. It is thought that in induction brazing the two materials are heated unequally, considering their different electrical resistivities, and this results in uneven or incomplete flow of the brazing alloy into the joint. Modification of an existing vacuum brazing furnace is in process; when completed, a better brazing procedure should be developed to produce reliable nickel-copper connections for CFTL FRSs.

1.2.4.8 Clad Roughening

Roughening of a specific portion of the cladding of a completed fuel rod simulator must be accomplished before the rods can be assembled

into CFTL bundles. General Atomic has WMC Grinding, Downey, California, as their subcontractor to perform rod roughening. Rejection of a completed rod because of clad roughening difficulty would result in a severe cost penalty.

Several ORNL rods were roughened by WMC Grinding to evaluate the process for completed rods. Results indicated that rod diameter variations would have to be maintained at ± 0.065 mm (± 0.0002 in.) or better if final roughened tolerances of ± 0.01 mm (± 0.0004 in.) were to be maintained. The normal tolerances on swaged fuel rod simulators is ± 0.025 mm (± 0.001 in.) and cannot be expected to improve beyond ± 0.015 mm (± 0.0005 in.). For this reason ORNL, GA, and WMC Grinding have evaluated both clad roughening and rod fabrication techniques to find methods to improve tolerances.

Evaluation of the grinding method resulted in an improved steady-rest method that should further reduce variations. The modification provides for rod diameter variation to be taken up by steady-rest wheel movement on the opposite side of the grinding wheel instead of by the present method. This will reduce rod diameter variation effects on groove depth. Further action on this method of improving tolerances has been suspended pending results on the nonswaged rods described in Sect. 1.2.4.9.

1.2.4.9 Nonswaged Rod Fabrication

Evaluation of a new concept in fuel rod simulators, the nonswaged rod, was initiated. Cladding of a nonswaged rod could be roughened prior to rod fabrication, eliminating the higher risk penalties associated with roughening a completed rod. With the nonswaged rod, the swaging step would be eliminated from the rod fabrication process, resulting in the following cost-saving and performance advantages:

1. elimination of handling and shipping rods to and from the site for clad roughening;
2. cost savings associated with reduction in the number of rods having roughening defects;
3. improved final OD and straightness tolerances;
4. improved instrumentation and heating-element dimensional location;

5. preset cold work in rod cladding, which is identical to GCFR fuel rod cladding; and
6. reduction in rod fabrication costs.

However, a nonswaged rod will almost certainly have reduced operational capability and less heat-flux profile uniformity compared with a swaged rod. Swaging increases boron nitride uniformity and density, which enhance thermal conductivity in the BN and result in lower internal temperature of the heating element for a given operation condition.

Boron nitride preforms with an 85%-of-theoretical density are now being produced, and continued improvements in preform filling techniques are taking place. The intent is to obtain a high, uniform BN density in a rod so that most of the program plan requirements can be attained with the nonswaged rod.

Material for fabricating nonswaged prototypic rods is on hand, and work will begin early in calendar year 1978.

1.2.5 Helium Circulators -- H. C. Young

1.2.5.1 Schedule

The schedule for the development and procurement of the circulators is shown in Table 1.4. The Circulator Design Report by Mechanical Technology, Inc. (MTI), was discussed in the previous progress report for the period ending December 31, 1976.⁴ No significant design changes have been made during the past year.

Table 1.4. Circulator Schedule

	Start			Complete			Progress
	Original	Most recent revision	Actual	Original	Most recent revision	Actual	
Phase I, design ¹	1/75		5/75	1/76		1/77	100%
Phase II, development ²	1/76		10/76	1/77	1/79		80%
Phase III, procurement ³	1/77	10/78		1/78	12/80		
Phase IV, assembly ⁴	1/78	12/80		1/78	1/81		

¹Phase I, design of circulator.

²Phase II, fabricate and test prototype circulator.

³Phase III, produce and test three CFB circulators.

⁴Phase IV, full supervision of onsite assembly and start-up.

Last year the estimated schedule for completion of Phase II was June 1978, based on an estimated expenditure of \$475 thousand in FY 1977. However, in February 1977 the circulator supplier, Mechanical Technology, Inc., Latham, New York, was requested to reduce FY 1977 expenditures on Phase II of the contract to \$375 thousand to match available funding. This reduction was accomplished by postponing placement of subcontracts for fabrication of the circulator test loop and test loop instrumentation until FY 1978. This resulted in an estimated 3-month delay in completion of Phase II to September 30, 1978. Recently the funding level for FY 1978 was reduced; also recently MTI informed us of an increase in its labor estimate to complete Phase II as well as that the delivery date for the prototypic pressure vessel and cooling water jacket has slipped progressively from November 1977 to April 1978. Delay in delivery of the cooling water jacket is particularly critical to MTI, because the motor stator must be assembled into the water jacket before bench testing of the circulator in air can be started. We estimate the reduced funding and other problems will delay completion of Phase II from September 1978 to April 1979.

We hope to start procurement of the pressure vessels and drive motors by October 1978 in advance of the procurement contract for the three production circulators, Phase III. The prototypic circulator will not have been tested by that date; however, any need for modifications to the circulator that are uncovered during the prototype testing are expected to result in changes to the circulator internals only, such as trimming the impeller, changing the diffuser, or reworking the bearings. The existing pressure vessel design can accommodate changes to the circulator internals, and tests of a ball-bearing-mounted prototypic motor indicate that the motor design is more than adequate.

1.2.5.2 Prototypic Pressure Vessel

The procurement package for the pressure vessel and cooling water jacket for the circulator prototype was approved by ERDA in January 1977 after about 6 weeks' delay while a nuclear indemnity clause in the low bidder's proposal was resolved. The subcontract for design and fabrication of the pressure vessel and motor cooling jacket (both to conform

to Section III, Class 2, of the ASME Code) was then placed by MTI with Buehler Nuclear Products (BNP), Indianapolis, Indiana.

The stress report for the pressure vessel design was submitted by BNP in late April 1977, and representatives from ORNL and MTI visited BNP in May to discuss the stress report and to review the schedule and quality assurance plans. Some additional work on the stress report was required to account for added loading on the vessel because of the cantilever support of the circulator internals from the vessel-end closure. MTI's vessel specification was revised to include this loading.

The seal weld design submitted by BNP did not meet ORNL's specified requirements that the seal weld lips should be designed to permit grinding away and remaking the seal a minimum of three times, nor did the design provide for register surfaces to permit alignment and welding of replacement weld lips. Buehler Nuclear Products subsequently provided a revised seal design and seal stress calculations that have been approved by MTI and ORNL.

A number of material certifications, fabrication procedures, and nondestructive test procedures were and are being submitted by BNP for review and approval on a continuing basis. Since the pressure vessel is being fabricated to Section III, Class 2, of the ASME Code, procedure approval is required both by MTI as the prime contractor for the circulator and by ORNL as owner-operator for DOE. This extended approval process plus the redesign of the vessel closure seal region has resulted in some delay, causing BNP to change the delivery date for the completed vessel from the original October 1977 date to January 1978. Representatives from ORNL and MTI visited BNP in late November 1977 to resolve some of the remaining review comments on the manufacturing plan, pressure test procedure, and packaging and shipping procedure. More recently BNP notified MTI that the delivery date for the pressure vessel and cooling water jacket would be April 1978. MTI protested that late delivery of the cooling water jacket will delay their scheduled bench tests of the circulator rotary assembly in air.

The cooling water jacket is to be assembled into the pressure vessel for pneumatic pressure testing, thus eliminating the chance that the cooling water jacket can be delivered to MTI in advance of the pressure vessel.

1.2.5.3 Prototypic Electric Motor

The requirements for the circulator drive motor and the motor design were discussed in the report for the period ending December 31, 1976."

A subcontract for fabrication of the prototypic motor was awarded in December 1976 by MTI to TASK Corporation, Anaheim, California. The contract included a review and extrapolation of the MTI motor-test data taken at 60 Hz to the 400-Hz requirements for the circulator motor and power supply. It also requested recommendations by TASK on improving the efficiency and power factor of the motor. The efficiency was improved by using AISI-type M-15 steel for stator laminations instead of the originally specified type M-19 steel to reduce core losses. The desirability of changing the solid rotor bar material from beryllium copper to pure copper was considered as another method of increasing motor efficiency; however, it was decided that the small gain in efficiency with copper rotor bars was offset by the higher strength of the beryllium copper.

Fabrication of the solid motor rotor and stator was completed by TASK in July 1977 in accordance with the design drawings and motor specifications provided by MTI. The motor design was obtained by MTI from General Electric by licensing agreement approved by ERDA at the time the CPFF contract for Phases I and II of the circulator contract was approved in June 1975. The design had originally been developed by General Electric for a motor that was used in the mid-1960s in a circulator for the Advanced Test Reactor, Idaho Falls.

The motor stator will be shrunk into the motor cooling jacket by MTI, and the gas bearings will be installed on the motor rotor shaft before operating the motor and rotor as a complete motor in air tests. Representatives from ORNL and MTI witnessed electrical acceptance tests of the stator at TASK and witnessed the brazing of the beryllium copper conductor bars to the collector rings in early July 1977. A corona starting voltage test in helium at 600 V was performed before the phase windings had been connected together to provide a corona test between phases. TASK then completed the interconnections between windings and performed the final vacuum-pressure class C varnish impregnation of the

stator. A megger and high-potential test was also performed on the stator. The stator and rotor were delivered to MTI in August 1977.

During the contract period, the TASK Corporation was purchased by Sunstrand Corporation, Rockford, Illinois. Although the fabrication of the motor for the CFTL prototype circulator was completed by TASK at Anaheim, the report covering the contractual requirements for an engineering evaluation of the data from tests of a prototypic GE motor conducted by MTI, analysis of the data, analysis of the motor constants, and recommendations for improving the efficiency of the motor were completed by Sunstrand. Copies of this report were received by ORNL and MTI in December 1977, thus completing the motor subcontract.

1.2.5.4 Power Supply for Circulators — T. L. Hudson

A specification for a variable-frequency solid-state power supply for the circulator drive motor was prepared by December 1976; however, confirmation of the motor current required for operation at 400 Hz was considered necessary for completing the specification.

TASK, the manufacturer of the prototypic motor (Sect. 1.2.5.3), was requested prior to start of actual motor fabrication to perform a review of 60-Hz test data taken by MTI with a prototype of the circulator motor to confirm MTI's calculated value of the current required for operation at 400 Hz. The TASK analysis, based solely on the specific engineering details shown by the MTI motor drawings, was in reasonable overall agreement with the MTI data extrapolated to 400-Hz operation from 60-Hz test data. Thus the results of these two independent analyses provided added assurance that the motor design was adequate and that the current value used to specify the power supply for the circulator motor was also adequate.

The specification for the power supply was then completed to obtain a solid-state variable-frequency power supply to convert 460-V (+3%), three-phase, 60-Hz (± 0 Hz) input power into an adjustable-frequency output with a nominal three-phase rating of 1.15 V/Hz over the range of frequencies from 0 to 400 Hz. The output power was to be suitable for providing stepless speed control from 0 to 24,000 rpm of a two-pole, three-phase constant-torque motor rated 206 hp at 393 Hz (23,500 rpm)

to drive a CFTL helium circulator and to allow acceleration to 3000 rpm in 1 s. In addition, the power supply was to be rated 250 kVA, at 460-V, 314-A, and 400-Hz output to allow some margin in motor rating. The power supply would consist of solid-state full-wave rectifier and inverter sections with current-limiting and self-protecting circuitry to withstand short circuits and to shut down the power supply safely in the event of input power failure. The power supply was to be a complete self-contained unit, assembled, and ready for operation. The power supply was to be tested at the manufacturer's plant with a ball-bearing fan-cooled motor of equivalent electrical design to the actual circulator motor. This test motor was to be supplied by UCC-ND.

The UCC-ND Purchasing Division received bids from three power supply manufacturers in March 1977. These bids were reviewed by the CFTL Program, and a fixed-price contract for the first of three power supplies required for the three CFTL helium circulators was placed in April 1977 with Servo-Optics Systems, then a subsidiary of TASK and now a subsidiary of Polyspede Electronic Corporation, Dallas.

A proposal was obtained from MTI for refurbishing the ball-bearing-mounted, solid-rotor prototypic motor having the equivalent electrical design of the CFTL circulator motor. A cost estimate was also obtained from TASK and other motor manufacturers for the manufacture of a 400-Hz test motor. It was determined that it would be more economical to purchase the MTI motor; however, because of CFTL funding shortages, it was decided to delay placing the order until FY 1978. This test motor was refurbished and delivered to Servo-Optics in late October 1977.

Mechanical Technology, Inc., has two specific requirements to protect the circulator gas bearings. One requirement is that the circulator rotary assembly be accelerated to 3000 rpm in 1 s. A second requirement is that the power to the circulator be interrupted if the circulator is started in reverse rotation, to prevent damage to the thrust bearing. A 120-tooth speed pickup gear and two magnetic pickups, identical to the parts to be used by MTI in the CFTL circulator prototype, were tested by the ORNL Instrumentation and Controls Division, and a solid-state device was developed to provide a signal to trip the power supply if the circulator is started in reverse. This equipment was sent to Servo-Optics and installed on the test motor.

Design and fabrication of the power supply were completed, and preliminary acceptance tests at Servo-Optics were witnessed by representatives from ORNL in December 1977. The completed power supply will be loaned to MTI for use in development testing of the circulator prototype and the three follow-on CFTL production circulators. The power supply will then be shipped to ORNL for installation at the CFTL site.

1.2.6 Instrumentation and Controls Development — H. J. Metz

1.2.6.1 Ultrasonic Helium Flowmeter

A prototype ultrasonic flowmeter was ordered from Scarpa Laboratories in June 1975, but the vendor was unable to deliver a complete working model. Incomplete portions of the meter were received at ORNL in late 1977, but the unit is inoperable. A review is in progress to determine if further work is justifiable on this specific ultrasonic flowmeter.

Due to delivery delays of the Scarpa meter, an engineering evaluation of the application of the ultrasonic flowmeter to measure helium flow rates in the CFTL was initiated with another vendor. Parametrics, Inc., of Waltham, Massachusetts, completed the evaluation in late December 1977, and ORNL will review it when the completed report is available.

1.2.6.2 Program Controllers — D. W. McDonald

An investigation into the use of 8-bit microcomputers in closed-loop control schemes for the CFTL was completed in 1977. The purpose of the investigation was to devise a general control scheme employing a microcomputer. The performance of this scheme would serve as a reference for program controlling for CFTL transients.

A process requiring closed-loop control of three parameters was chosen. It was assumed that 16-bit binary numbers would be required to satisfy accuracy constraints; thus both the set point and feedback signal were represented as 16-bit numbers. The control scheme involved subtracting the two 16-bit numbers to generate an error signal, performing a digital-to-analog conversion on the error signal, and outputting this signal to an appropriate interface circuit. It was further

decided that both the set point signal and the actual controlled parameter value would be displayed as five binary coded decimal (BCD) digits. A simplified block diagram of the system is shown in Fig. 1.18.

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Fig. 1.18. Microprocessor-Based Closed-Loop Controller.

The microcomputer is a Control Logic, Inc., implementation employing an Intel 8080 microprocessor. All software was written using an 8080 compiler, PLM. The entire algorithm to control the three parameters takes about 10 ms to perform. The algorithm includes decoding the three setpoint and feedback signals; performing the three 16-bit subtractions; storing the setpoints and actual values of the parameters in memory for later display; performing the digital-to-analog conversion, the binary-to-BCD conversion, and all input and output multiplexing; and displaying the required output values and doing miscellaneous control and timing chores. Since a rule of thumb in process control requires that a controller be at least ten times faster than the process, our microcomputer closed-loop control scheme would be limited to processes such as furnace controllers and slow-moving, heavy-machinery controllers having a bandwidth of 0 to 10 Hz. The estimated bandwidth of the helium flow control valves for the CFTL is 12 Hz. Since the valve controllers

will require proportional, integral, and derivative (PID) control modes rather than the proportional-only mode used in our investigation, the three-mode algorithm would be significantly longer than 10 ms; therefore, closed-loop control of the valves during fast transients with a microcomputer seems marginal. It is currently anticipated that a microcomputer will be used to generate an analog setpoint and that an analog PID controller will be used to control the valves.

1.2.6.3 Fast-Scan Data Acquisition System — K. J. Cross

The configuration of the CFTL fast-scan data acquisition system (FSDAS) was determined. It will consist of a computer-based data acquisition system and an automated calibration and verification system.

1.2.6.3.1 Computer-Based Data Acquisition System. The computer-based data acquisition system is the prime data handler in the CFTL. The system will acquire, record, and perform on-line processing of as many as 640 measurement channels. The major components of the system include:

1. a minicomputer with two 7-million-word disk memories, floating point processor, printer/plotter, four user terminals, and a real-time operating system;
2. two 800- or 1600-bits-per-inch (bpi) magnetic tape transports to record data;
3. high-speed (20,000-channel-per-second) analog input system with multiplexers for 640 analog input channels; and
4. digital input-output for use with alarms and for interfacing with the automated calibration and verification system.

The system will be capable of recording and scanning data from the 640 measurement channels by recording the data on magnetic tape and comparing all measurements to predefined limits on a per-channel basis. This latter feature will be used as one channel of the two-channel protective system, which will shut the loop down when abnormalities are detected.

The Automatic Data Processing (ADP) proposal has been prepared and submitted for the computer-based data acquisition system. Included in

the ADP proposal is a request for sole-source procurement of a major portion of the equipment. This request is based on achieving compatibility with the data acquisition system used with the Thermal Hydraulic Test Facility of the Blowdown Heat Transfer (BDHT) Program. Such compatibility will considerably reduce the software design effort and hardware acceptance and shakedown testing and will provide for flexibility in the maintenance of both data acquisition systems.

1.2.6.3.2 Automated Calibration and Verification System. Past experience has shown that there is a formidable amount of staff time and craft costs associated with the manual calibration of a large number of analog input channels and the functional verification of a large number of instruments. A system that has been proposed and is in the preliminary design stages will perform these operations automatically. The system will have the capabilities to:

1. calibrate and verify each analog input channel in the computer-based data acquisition system;
2. verify functionally the integrity of most field instruments, that is, assure that they are connected and operating properly; and
3. calibrate critical instruments in situ, for example, resistance temperature devices (RTDs), strain gages, and pressure transducers.

The proposed system is being developed for use by several projects at ORNL in addition to the CFTL. Certain variations will be implemented in the system to accommodate the specific complement of CFTL instruments.

1.2.6.4 Small-Diameter Thermocouples — R. L. Anderson

Work has continued on the characterization of small-diameter (0.5-mm) thermocouple materials to be used in the CFTL fuel rod simulators (rods). This effort has included (1) analysis of the source of errors, (2) determination of the reliability of small-diameter thermocouples under conditions of CFTL programmed tests, and (3) collection of sufficient data on several combinations of thermocouple and sheath materials to permit selection of the most suitable thermocouples for CFTL use.

Testing of small-diameter thermocouple materials is necessary because the CFTL will require both higher-temperature service and more accurate measurement of temperatures than did previous thermocouple users.

The small dimensions, particularly of the thermocouple wires, cause these thermocouples to behave differently than larger-sized wires in their electrical, mechanical, and chemical properties.

Testing in 1977 included (1) the development of an analytical model to calculate and analyze the electrical effects (leakage and shunting) caused by the reduced electrical resistance of the insulation between thermocouple wires and between wires and sheath at temperatures up to 1000°C; (2) the effect of the choice of sheath materials on the decalibration of small-diameter thermocouples, which included the thermocouple-sheath combinations given in Table 1.5; and (3) decalibration tests of 1, 2, and 4 in Table 1.5 under the most severe high-temperature conditions anticipated in the CFTL experiments (50 h at 1150°C) short of the approach-to-clad melting.

Table 1.5 Thermocouple Material Combinations Evaluated for Decalibration at High Temperature

-
1. Type K in type 304 stainless steel sheath^a
 2. Type K in Inconel 600 sheath
 3. Type S in type 304 stainless steel sheath^b
 4. Type S in 90% platinum-10% rhodium sheath
 5. Type S in 80% platinum-20% rhodium sheath
 6. Type S in Inconel 600 sheath
 7. Type B in type 304 stainless steel sheath^c
 8. Type B in Inconel 600 sheath
-

^aType K = Chromel vs Alumel.

^bType S = platinum-10% rhodium vs platinum.

^cType B = 94% platinum-6% rhodium vs 70% platinum vs 30% rhodium.

Because the decalibration, particularly of type K materials, has been shown to be at least partially dependent on the presence of air and water sorbed on the MgO insulation, an inert-atmosphere glove box was set up to make thermocouples and test samples from bulk small-diameter material in a dry argon atmosphere. This facility is particularly useful for the fabrication of test thermocouples from bulk materials that have been manufactured in argon.

1.2.6.4.1 Analytical Model for Electrical Shunting and Leakage.

Earlier experiments had shown potentially large errors due to electrical leakage and shunting (ref. 4, pp. 78-83). An analytical model, based on electrical transmission-line theory for three parallel conductors, was developed for small-diameter sheathed thermocouples. In this model, the thermocouple is divided into small sections, Δx as shown in Fig. 1.19, and the sections cascaded together with a given temperature profile to obtain a solution, which gives a good approximation of the errors caused by either electrical leakage or electrical shunting or by both.

The necessary parameters (resistance per unit length, the Seebeck coefficients, and the conductances per unit length) were all determined

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Fig. 1.19. Approximate Electrical Model of a Short Section of a Thermocouple of Length, Δx . V_C , V_A , and V_S represent the voltages per unit length generated by the Seebeck effect; r_C , r_A , and r_S represent the series resistances per unit length of the Chromel, Alumel, and sheath, respectively; g_{CS} , g_{AS} , and g_{CA} represent the shunt conductances per unit length for Chromel to sheath, Alumel to sheath, and Chromel to Alumel; v_C and v_A represent the voltages on the thermocouple, Chromel to sheath, and Alumel to sheath; and i_C , i_A , and i_S represent the currents in the Chromel, Alumel, and sheath elements respectively.

experimentally at several temperatures. Furthermore, the experiment was conducted in such a way as to provide a check on the model. A comparison of the calculated and measured errors resulting from electrical leakage is shown in Fig. 1.20. The agreement is excellent, particularly considering that the input parameters were observed to change with time at higher temperatures during the experiments.

Fig. 1.20. Calculated and Measured Electrical Leakage Errors vs Sheath Current (Error of 40 μ V = 1 K).

The model was also used to calculate the effect of shunting as well as a related phenomenon, the virtual junction. This is illustrated in Fig. 1.21 in which the measuring junction is located as desired in the temperature profile; the leads extend leftward. When the measuring junction is moved from a position at about 1 m to a position at 2 m (i.e., into an increasing temperature profile), the error is negative due to loss of signal resulting from increased conduction between the thermocouple wires. When the measuring junction is moved from the 2-m toward the 3-m position in the temperature profile, the error becomes

Fig. 1.21. Calculated Error in Indicated Temperature as the Measuring Junction is Moved Through the Temperature Profile Indicated Above.

positive and large, the thermocouple tending to indicate the higher temperature at the 2-m position. These results have been reported in detail by Roberts and Keller.⁵

1.2.6.4.2 Comparison of Inconel and Stainless-Steel-Sheathed Thermocouple Errors. The decalibration experiments reported previously (pp. 75-78, ref. 4) were extended to 1100 and 1150°C for 50 h each, which has been estimated to be an adequate operating time at temperature for many of the CFTL margin undercooling tests.

In these experiments, the decalibration was observed (1) with the thermocouple in a fixed position with respect to calibrating furnace and (2) by shifting the thermocouple with respect to the furnace. The

decalibration errors observed with the thermocouple in a fixed position (i.e., fixed temperature gradient) will be referred to as decalibration errors of the first kind. The decalibration errors observed when the thermocouple is shifted with respect to the temperature gradient in the calibrating furnace will be referred to as decalibration errors of the second kind. Decalibration errors of the second kind are of prime importance for the CFTL, because, in the CFTL experiments, the temperature gradients with respect to the rod thermocouples will change with changes in bundle power and helium coolant flow.

The results of these experiments for a type K thermocouple in a type 304 stainless steel sheath are shown in Fig. 1.22. At the end of the 50-h period, the thermocouple was indicating a temperature error of -13.5°C . During the experiment, the outputs of the two individual elements of the thermocouple were measured against platinum, and the deviations (in arbitrary units) of the Chromel vs platinum and Alumel vs platinum are also shown. For the first 30 h, the drift is due almost entirely to the changes in the Alumel. After 30 h, however, there was an effect due to changes in the Chromel, in opposition to the drift produced by the Alumel. These decalibration errors are of the first kind.

Fig. 1.22. Changes Observed in Type K Thermocouples Sheathed in Stainless Steel During a 50-h Exposure at 1150°C .

A type K thermocouple sheathed in Inconel 600 was included in the same experiment. The results of the 50-h drift test on this thermocouple were markedly different from the type K in stainless steel. As shown by Fig. 1.23, over the entire 50-h period, the temperature indicated by the thermocouple changed less than 2 C°.

Fig. 1.23. Decalibration Observed in a Type K Thermocouple Sheathed in Inconel 600 During a 50-h Exposure at 1150°C.

Such behavior does not give a true picture of the accuracy of temperature measurements made in an application such as the CFTL. The Inconel-sheathed type K thermocouple showed as much as 15% decalibration (change in sensitivity) near the measuring junction. By shifting the thermocouple in the furnace gradient ± 2.5 cm, the indicated temperature was changed by as much as 6.6 C°. This error is of the second kind.

The apparent ± 1 C° stability of the type K in Inconel is valid for a fixed temperature gradient only. Displacement of the temperature gradient such as will occur in CFTL experiments with different flows, different power levels, and during transient measurements will result in decalibration errors of the second kind in temperature measurements, which will not be detected by an "in-place" or steady-state calibration.

Another experiment was run to obtain further information on the effects of decalibration errors of the second kind. In this experiment, two type K thermocouples, sheathed in Inconel 600, were carefully calibrated from room temperature to 1100°C. The thermocouples were held at 1100°C for 50 h and then cooled to ~ 300 °C. These data are indicated by curves marked A in Fig. 1.24. The calibration assembly was then

Fig. 1.24. Calibration Curves Measured on a Type K Thermocouple Sheathed in Inconel 600.

shifted out of the calibrating furnace by 9 cm, placing the temperature gradient of the calibrating furnace in a region of the thermocouples that had been at 1100°C for 50 h. A new calibration was subsequently run from 300 to 1100°C and back. These data are indicated by curve B in Fig. 1.24. The thermocouple assembly was then shifted into the furnace 8 cm beyond the original position, so that fresh, virgin material was placed in the temperature gradient of the calibrating furnace. A third calibration was run and these results, designated in Fig. 1.24 by curve C, essentially reproduce the original calibration curves A, as would be expected. At 600°C, the difference between curves A and C and curve B is about 10 C° or about 1.7%.

1.2.6.4.3 Error Analysis. GA memo 76/006227, "Requirements for CFTL and Test Bundles," sets the desired temperature measurement accuracy for the CFTL experiments as $\pm 8^\circ\text{C}$ to 800°C and as $\pm 15^\circ\text{C}$ for temperatures above 800°C . Table 1.6 lists the error sources to be considered in estimating the temperature-measurement uncertainties for the system.

Table 1.6. Error Sources for Thermocouples
in the CFTL

Error sources	Best case estimate of uncertainty ($^\circ\text{C}$)	
	Type K	Type S
1. Extension lead wires, calibrated	± 0.5	± 0.5
2. Reference junctions	± 0.1	± 0.1
3. Data acquisition system, for $T < 1000^\circ\text{C}$	± 2.4	± 2.4
4. Electrical noise, with thermo-stated active filters	± 1	± 1
5. Calibration, of a single thermocouple	± 1.5	± 1
6. Decalibration, at 1100°C after 50 h at 1100°C	± 15	± 5
7. Thermal shunting	$< \pm 1$ (steady state) Can be as much as 30°C during transients	
8. Electrical leakage, at 1200°C	± 2	± 2
9. Electrical shunting, including virtual junctions	Up to $\sim 30^\circ\text{C}$ — depends on temperature, temperature profile, and position of thermocouple above 1000°C	

Since many of these errors are temperature dependent, they are perhaps better visualized in graphical form as in Figs. 1.25 and 1.26, which treat only the error sources 1 through 6. The central portion of the error bands include the uncertainties contributed by the extension leads (calibrated), the reference box, and the calibration of single thermocouple, error sources 1, 2, and 5 respectively. Differences in

Fig. 1.25. Graphical Representation of the Sources of Errors 1 Through 6 in CFTL FRS Thermocouple Temperature Measurements Using a Type K Thermocouple in an Inconel 600 Sheath. K_{O-d} refers to decalibration errors resulting from the order-disorder transformation; K_I and K_{II} refer to decalibration errors of the first and second kind respectively.

thermocouples have not been included in these figures. The second band of uncertainties added to the above uncertainties is caused by the data acquisition system (DAS). This includes the $\pm 1^\circ\text{C}$ uncertainty caused by the active filters used for noise reduction. The high-speed analog-to-digital (A-to-D) converter that will be incorporated in the DAS has a $\pm 0.25\%$ range of uncertainty associated with ranges of 10, 20, 40, 80, 120, etc., mV. The steps in the DAS uncertainty curves are due to the range changes in the A-to-D converter as the output of the thermocouples

Fig. 1.26. Graphical Representation of the Sources of Errors 1 Through 6 in CFTL FRS Thermocouple Temperature Measurements Using a Type S Thermocouple in a 90% Platinum-10% Rhodium Sheath. S_I and S_{II} refer to decalibration errors of the first and second kind respectively.

increase with temperature. At this point, an appreciable difference begins to emerge in the temperature measurement uncertainties associated with type S and type K thermocouples. The greater sensitivity of the type K (40 mV/°C for type K vs 10 mV/°C for type S) does not result in a greater temperature measurement accuracy, primarily because, with type K at about 270°C, the A-to-D converter must shift to the less sensitive 20-mV range, which consequently has a larger uncertainty associated with it. At about 485°C, the range must be changed to 40 mV. With the type S thermocouple, however, the most sensitive 10-mV range of the A-to-D converter is used up to 1035°C.

Errors due to decalibration. Type K thermocouples are subject to decalibration due to formation of short-range order in the Chromel element. This type of ordering is both temperature and time dependent. Over a long period of time, the maximum extent of the error caused by ordering appears to occur at about 300°C. However, over a short period, the maximum effect occurs at about 400°C. Above 600°C, the short-range order is destroyed. The net effect is to contribute an uncertainty to temperatures measured with type K thermocouples, which approaches 1.1% of Celsius temperature at temperatures up to 600°C.

Because the order-disorder transformation is not only time and temperature dependent but is also reversible, steady-state in-situ recalibration of CFTL FRS thermocouples, as has been proposed from time to time, would be uncertain by an amount represented by the line designated K_{O-d} in Fig. 1.25. As shown in Fig. 1.26, the type S thermocouple has no order-disorder transformation; consequently, no uncertainty results from this type of effect.

At higher temperatures, decalibration will occur because of compositional changes in the thermoelements caused by reaction of the thermocouple alloys with (1) the atmosphere inside the sheath, (2) the insulation material, (3) impurities in the insulation, (4) impurities that diffuse across the insulation from the sheath, and (5) preferential evaporation of one or more components of the thermocouple alloy. The curves designated K_I and K_{II} in Fig. 1.25 were measured at the end of a 50-h drift test in which type K thermocouples in Inconel sheaths were held at 1100°C for 50 h. The data for curve K_I were taken on cooling from 1100°C at the conclusion of a 50-h test. Curve K_I represents decalibration errors of the first kind. Curve K_{II} was taken in a subsequent calibration run for which the test thermocouples had been shifted out of the calibration furnace by 9 cm so that portions of the thermocouple which had exposed the 1100°C treatment were placed in the steep temperature gradient of the calibration furnace. Curve K_{II} represents decalibration errors of the second kind. The large difference between curve K_I and curve K_{II} demonstrates that recalibration at steady state can give only misleading and erroneous results. Decalibration of a thermocouple is a function of position. The temperature-emf relationship of a thermocouple is therefore also dependent on the shape and

position of the temperature gradients along its length. Because the decalibrated thermocouple is sensitive to the location and shape of the temperature gradient imposed along its length, there is no quantitative means of relating the temperature-emf relationship in one temperature gradient to the relationship in any other temperature gradient. Thus efforts to reduce temperature measurement errors caused by decalibration must be limited to the selection of thermocouple- and sheath-material combinations and/or manufacturing procedures that minimize decalibration.

Comparison of Fig. 1.25 with 1.26 shows that, although small-diameter type S thermocouples in platinum-rhodium sheaths do have decalibration errors of both the first (S_I) and second (S_{II}) kinds after high-temperature exposure, the effect is much less than for type K and, furthermore, that type S does not have the insidious order-disorder transformation.

Figures 1.25 and 1.26 refer to a single calibrated thermocouple. In practice, it would be impractical and in fact undesirable for the CFTL to calibrate each thermocouple installed in a rod. Therefore, we have prepared Figs. 1.27 and 1.28 to show the results of including the standard Instrument Society of America (ISA) tolerance in the cumulative uncertainties for the type K and type S thermocouples respectively. The ISA allowable tolerance for special-grade type K is $\pm 2.2^\circ\text{C}$ to 277°C and 0.4% of T from 277 to 1250°C . This is indicated by the curves marked $3/8$ in Fig. 1.27. The ISA tolerance for standard-grade type S thermocouples is $\pm 1.5^\circ\text{C}$ to about 570°C and $1/4\%$ from 570 to 1450°C . This is indicated by the curves marked $1/4$ in Fig. 1.28. The shaded areas in these figures indicate the areas in which the cumulative uncertainties exceed the CFTL tolerance limits, indicated by the dashed lines. The uncertainties caused by compositional decalibration (as opposed to order-disorder decalibrations) are indicated by K_{II} and S_{II} in Figs. 1.27 and 1.28 respectively. Because the CFTL test plans call for the higher temperature experiments to come at the end of each test, the region between 300 and 800°C where these uncertainties exceed the CFTL tolerances are probably not of practical significance since this decalibration will occur only after high-temperature exposure. The order-disorder transformation in the type K materials, however, exceeds the

Fig. 1.27. Cumulative Uncertainties Expected to Result from Using Uncalibrated Type K Thermocouples in Inconel 600 Sheaths Purchased with ISA 3/8% Tolerance for Special-Grade Type K Materials.

allowable tolerance in the region of prime importance to the CFTL experiments. Furthermore, the errors due to thermal shunting, electrical leakage, and electrical shunting have not been included in Figs. 1.25, 1.26, 1.27, and 1.28. Accurate evaluation of these error sources will require further analytical and experimental work.

1.2.6.4.4 Noble-Metal Thermocouple Procurement. Early in 1977, orders were placed with three manufacturers to produce 0.5-mm-diam noble-metal thermocouples with a variety of different sheath materials and processing procedures. Table 1.7 lists these materials.

Fig. 1.28. Cumulative Uncertainties Expected to Result from Using Uncalibrated Type S Thermocouples Sheathed in 90% Platinum-10% Rhodium Purchased with ISA 1/4% Tolerance for Standard-Grade Type S Materials.

All manufacturers experienced difficulty in drawing noble-metal-sheathed materials to diameters smaller than about 1 mm. The material supplied by Harrison proved to have broken conductors and was therefore useless. The materials supplied by Control Products were initially of short lengths (4 to 20 cm long), insufficiently long for experimental purposes. Continued efforts by this company, however, produced useful lengths of materials (>1.5 m), which were of high quality. Figure 1.29 shows the calibration curves taken on one sample of this material; the deviations, Aa, Ab, B, and C, from the International Practical Temperature Scale, 1968, are identical to that of a high-quality "nominal"

Table 1.7. Materials from Which Manufacturers Were Requested to Produce 0.5-mm-diam Noble-Metal Thermocouples

Manufacturer	T/C type	Sheath	Processing
Gordon	S	Type 304 stainless steel	Air
Gordon	S	Type 304 stainless steel	Argon
Gordon	S	Inconel 600	Air
Gordon	S	Inconel 600	Argon
Gordon	B	Inconel 600	Argon
Gordon	B	Inconel 600	Air
Gordon	B	Type 304 stainless steel	Air
Gordon	B	Type 304 stainless steel	Argon
Gordon	S	Platinum-20% rhodium	Air
Gordon	S	Platinum-20% rhodium	Argon
Gordon	B	Platinum-20% rhodium	Air
Gordon	B	Platinum-20% rhodium	Argon
Control Products, Inc.	S	Platinum-10% rhodium	Air
Harrison	S	Platinum-10% rhodium	Air

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Fig. 1.29. Calibration Curves Measured on a Type S Thermocouple Sheathed in 90% Platinum-10% Rhodium.

type S bare-wire thermocouple. Claud S. Gordon experienced difficulty in producing the noble-metal-sheathed materials and failed to supply the argon-processed material. Gordon initially experienced some difficulty with wire breakage in producing the air-processed materials; however, by using wire from another supplier, they were able to produce the air-processed materials in approximately 7.6-m lengths.

The experience in procuring noble-metal-noble-metal-sheathed thermocouples showed that, while not straightforward, these materials can be produced in 0.5-mm-diam sizes and in lengths sufficient for CFTL FRS use.

1.2.6.4.5 Inert-Atmosphere Glove Box for Thermocouple Fabrication.

Because of the hygroscopic nature of magnesium oxide powder used as the insulating material in most sheathed thermocouples, moisture is absorbed during handling and fabrication. This problem is acute in handling small-diameter materials because a substantial amount of time is required in fabrication of junctions, end seals, etc. Moisture adsorbed by the MgO can lower its insulation resistance several orders of magnitude. Of more significance, however, is that the corrosion of the nonnoble-metal thermocouple alloys is considerably enhanced at high temperature by the presence of this water vapor trapped inside the sheath. An inert-atmosphere glove box is therefore very useful for fabricating small-diameter thermocouple materials under conditions that exclude uncontrolled moisture from the thermocouples.

The inert-atmosphere glove box has been received and put into operation. It contains the necessary welding and assembly equipment to fabricate small-diameter thermocouples, and the first few thermocouples have been fabricated for testing. Additional samples, particularly of materials that have been produced under inert-atmosphere conditions, are currently being fabricated for testing in the near future.

1.2.6.5 Thermocouple Noise and Noise Filters - D. W. McDonald

Specifications for CFTL thermocouple signal-conditioning instrumentation were completed in 1977. The signal conditioning includes amplification with high-quality, low-drift amplifiers and multiple pole

filtering. The specifications have already been used in purchasing thermocouple signal conditioners for other projects at ORNL.

An in-house signal conditioner was developed to meet the specifications. It has been operated with a CFTL rod in the dc-leakage test and was demonstrated to reduce significantly induced thermocouple noise as detected by the data acquisition system. The conditioner consists of a printed circuit board designed to amplify and filter three thermocouple channels. The board can be fabricated in quantities at a lower cost than commercially available conditioners. This conditioner is currently being used by other ORNL researchers, and their feedback will be employed to make improvements.

1.2.6.6 Structurally Related Instruments for Test Bundles -- H. J. Metz

The status of structurally related instrument development is discussed below. In the first quarter of FY 1978, all development funding for this work was halted temporarily, necessitating the cancellation of work orders and outstanding purchase requisitions.

1.2.6.6.1 Hanger-Rod Axial Load. Although strain gages and compatible resistance thermometers were purchased, when development funds were reduced, the devices were diverted to another program. No reportable progress was made.

1.2.6.6.2 Edge-Rod Bowing. Proximity sensors of the capacitance type are available and seem to be suitable for use in the test bundle; they are expensive. Design and construction of a model of the 91-rod bundle, three spacers long and three rows deep, was initiated, and proximity sensors were about to be requisitioned when funding ceased.

The use of flash radiography to observe edge-rod bowing was considered. Tests were made with a CFTL bundle mock-up using 600-kV x-ray machines at Los Alamos National Laboratory. The radiograph made with the 24-mm (1-in.) flat plate that simulated the test vessel was so lacking in definition that flash radiography is believed unacceptable for rod-bowing observations.

A different mock-up was taken to Balteau Electric Company in Stamford, Connecticut, to evaluate its 420-kV system with image enhancement. A 460-mm-long (18-in.) section of the pressure vessel and a 152-mm-long (6-in.) section of a 91-rod test bundle were made, using boron-nitride-filled tubes as fuel rods. The radiography was unacceptable.

Several of the 152-mm (6-in.) rod sections were sent to Philips Electronics in Hamburg, West Germany, for tests with their image-enhanced fluoroscopy system. Although this work is proceeding, no information was developed during this reporting period.

K. K. Klindt, ORNL Quality Assurance and Inspection, who is leading the x-ray and fluoroscopy work, suggested that a reduction of test-vessel wall thickness to 13 mm (1/2 in.) could make image-enhanced fluoroscopy a much better candidate to measure inner- and outer-duct bowing, axial differential expansion, and rod bowing. The reduction of test-vessel wall thickness using a low-alloy high-strength steel is being studied by the CFTL design engineers.

If the thin vessel wall can be used, fluoroscopy may permit measurement of changes in rod position to within 0.25 mm (0.010 in.). Two image-enhanced fluoroscopic systems would be used, one installed in a fixed position at the bottom end of the test bundle to measure duct movement and axial expansion. The other system would be flexible, so as to move vertically and to rotate around the vessel's vertical axis to see inner- and outer-duct bowing and rod bowing.

1.2.6.6.3 Axial Differential Expansion - Photogrammetry. A purchase order was placed with Bendix Research Laboratories in Southfield, Michigan, to do an engineering evaluation of the use of photogrammetry in the CFTL. It is estimated that photogrammetry might provide measurement of rod and duct elongation to about 0.15 mm (0.006 in.) with reference to the focal plane of the optics and to about 0.05 mm (0.002 in.) of rod elongation if the duct were used as a reference plane. However, the purchase order was cancelled.

1.2.6.6.4 Acoustical Measurements. A microphone that might be suitable for use in the CFTL was requisitioned for test and evaluation. The requisition was cancelled due to lack of funds.

1.2.5.6.5 Accelerometers. An accelerometer that might be used in the CFTL was requisitioned for test and evaluation. The requisition was cancelled.

1.3 ADVANCED CONCEPTUAL DESIGN OF THE LOOP - C. W. Collins, W. R. Huntley, and A. G. Grindell

1.3.1 Mechanical Design

1.3.1.1 Test Vessel - J. A. Seneker

Advanced conceptual design began this year on the test vessel, which provides the pressure boundary containment for the test bundles. As shown in Fig. 1.30, the diameter of the vessel is set by the largest bundle, the full-size blanket bundle, which would be placed off center in the vessel to permit bowing of the bundle duct up to 80 mm during skewed power tests. Layouts indicate that a vessel diameter equal to that of a 35-cm-ID (16-in. schedule 80) pipe would be sufficient to accommodate any test bundle and its associated duct and anticipated bowing. A vessel-wall thickness (22.2 mm) of 304 stainless steel would be required to withstand the design conditions of 11.7 MPa at 399°C. To accommodate anticipated duct bowing, the centerline of the blanket bundle will need to be offset from the centerline of the test vessel by 40 mm; to accommodate 100 mm of duct bowing anticipated for the 91-rod fuel bundle, that bundle would also need to be offset from the centerline of the vessel. However, the 37- and 61-rod fuel bundles will probably be concentric with the vessel centerline. The length of the test vessel, as requested by General Atomic, will accommodate test bundle lengths up to 3000 mm and will also provide for inlet and outlet nozzles and top and bottom vessel closures. Overall length of the test vessel with the bottom closure installed will be about 3800 mm. The test bundles will be suspended vertically from the top of the test vessel.

Reducing Weldolet-type vessel nozzles were chosen, which would adapt the loop piping to the 16-in.-diam vessel. This type of nozzle will readily withstand the expected nozzle loadings and will provide a

Fig. 1.30. Cross-Section Views of Test Vessel with Various Test Bundles Installed.

standard component that can be welded easily to both the vessel and the piping.

After study of the top and bottom vessel closures, it was determined that the closures should be Gray-Loc flanges that use a coated-metal seal ring rather than standard bolted flanges that use a seal weld at the parting line. The Gray-Loc has less bulk and a smaller diameter and should require less time for installation and removal than the seal-welded standard flange. The flange parts would mate to schedule 50 pipe, would be of AISI type 304 stainless steel, and would be designed to ASME Section III, Class 2, requirements for the vessel design conditions.

The penetrations for up to 91 fuel rod simulators, which make a tube sheet of a portion of the top blind flange closure, weaken the closure. The stresses in the tube sheet were analyzed using the finite element computer program FEATS. Also a thermal analysis tool, FEATS was used to calculate temperatures throughout the tube sheet that resulted from resistance heat losses generated in the fuel rod simulators, particularly over the length of rod enclosed in the tube sheet. This analysis indicated that, for a standard blind hub of about 100-mm thickness, the temperatures of both the rods and the tube sheet metal exceeded that allowed. A modified blind flange was analyzed, in which a 165-mm-diam integral tube sheet of recess form was machined, leaving a tube sheet thickness of 38 mm. Results indicated that temperatures in the tube sheet were within the allowable range and that this design would withstand the design pressure. However, a recent thermal analysis, using the HEATING 5 program conducted in conjunction with the rod connector development work, has revealed temperatures in the tube sheet which are somewhat higher than those predicted by the FEATS analysis. Work is currently under way to resolve this inconsistency and to determine which method most accurately models the actual system.

The outlet section of the test vessel must contend with very high helium temperatures from the bundle exit during certain tests. Preliminary design of this part of the vessel was initiated with the primary emphasis on methods of mixing the main bundle helium with the attenuator helium before the flow reaches the heat exchanger, HX-1, and on methods of protecting the walls and flanges of the vessel from the very high

temperature helium. The mixing concept that appears to be the most promising is the use of a Kenics Turbu-Mixer located in the outlet pipe that connects the test vessel with the heat exchanger. This device consists of four fixed right-hand and left-hand helical elements that alternately bisect the flow, thereby resulting in a homogeneous mixture of the main and attenuator flows in a short distance. Advantages of this concept include low-pressure drop and simple and standard parts in addition to permitting the location of the mixer just downstream of the vessel, thereby leaving clear access in the vessel outlet (lower) plenum for instrumentation and bundle-support equipment. Protection for the vessel wall and flanges as well as the outlet pipe is provided by an Inconel liner, which serves as a sacrificial shield to absorb the thermal shock of the alternating impingement of high- and low-temperature helium and acts as an insulator for the pressure-containing boundary. Sizing of the mixer device and calculations of the pressure drop through the mixer were conducted. The optimum thickness of the Inconel liners will be determined to predict their thermal dampening capabilities.

Design of the inlet plenum region of the test vessel was also begun. Methods of distributing the helium flow around the test bundle and means of providing a seal between the main flow and attenuator flow were investigated. The inlet plenum contains two concentric baffles around the test bundle to increase the length of the helium flow path and thereby assist in distributing the coolant uniformly around the perimeter of the bundle inlet. Pressure-loss coefficients and flow-induced vibration frequencies of these baffles have been analyzed. Concepts of several methods of sealing the main flow from the attenuator flow are currently under investigation. Trade-off studies between the various methods are being conducted with respect to expected leakage, ease of fabrication, and installation. The two most promising concepts are a piston-ring seal design and a low-compression metallic O-ring or machined metal V-seal design.

Reduction of test vessel wall thickness is desirable to improve bundle surveillance by the x-ray fluorescence screen technique. One means of accomplishing this is to fabricate the vessel of a stronger material. The use of a high-strength material, SA533, is currently

under investigation as a construction material. A finite-element analysis of the region where the vessel is joined to the stainless steel closure flanges is being conducted to predict the stresses in the dissimilar metal joint when subjected to the design conditions.

1.3.1.2 Heat-Removal System — W. A. Hartman

A cost estimate of the mechanical engineering portion for construction of the CFTL loop was made, based on the advanced conceptual design reported below. Preliminary designs for the helium-to-air heat exchangers were completed. The optimum configuration was determined to be a staggered arrangement of serpentine tubes across which air is forced in the upward direction. The number of tubes and passes per tube were determined to provide the necessary heat transfer area while maintaining an acceptable air-side pressure loss. This was done for the main (large) heat exchanger (HX-1), the bypass (small) heat exchanger (HX-2), and an auxiliary heat exchanger (HX-3) that was found to be unnecessary as discussed later.

Preliminary designs were also developed for both the large and small cooling-air handling systems. Air supply requirements were determined, and equipment was selected to meet these requirements. HX-1 removes heat from the loop over a wide range of steady-state bundle power operations. Using vendor data for fan performance, it was found that cooling-air flow through HX-1 can be controlled over a range of 18:1 by combined use of variable inlet vanes and a 4:1 variable-speed drive. Natural convection is planned for heat removal below the range covered by forced convection of air. It is planned to modulate natural convection, using louver-type dampers in the air handling duct-work systems.

1.3.1.3 Piping and Equipment Layout — W. A. Hartman

During advanced conceptual design, the preliminary mechanical equipment arrangement and primary helium piping layout shown in the flow diagram in Fig. 1.31 were developed. Vertical clearance needed for bundle handling requires an opening in either the roof or the second

floor of Building 9201-3. The options were evaluated, and the roof opening was selected as the reference design because lowering the test vessel restricts access for test instrumentation. The helium circulators were relocated slightly to permit maintenance access. The piping layout was refined to facilitate valve trim change-outs and to satisfy flow sensor requirements for straight runs of pipe 15 diam upstream and 4 diam downstream of the sensor. The piping layout was modified to reflect removal of the heat exchanger (HX-3) as discussed below. Relocation of equipment is restricted by structural features of the building, and Y-12 Civil and Architectural Engineering was requested to ensure that the building structure provides suitable support for the loop equipment. The mechanical equipment arrangement is shown in plan and elevation views in Figs. 1.32 and 1.33.

Fig. 1.31. Flow Diagram - Core Flow Test Loop.

Fig. 1.32. CFTL Mechanical Equipment Arrangement Plan View.

Fig. 1.33. CFTL Mechanical Equipment Arrangement Elevation View.

The previous conceptual design included a 400-kW heat exchanger (HX-3) in parallel with the 4-MW main heat exchanger (HX-1) to extend control of heat removal to low test bundle power and helium flow rate. However, dynamic analysis of the loop has shown the loop thermal time constants to be large enough to mitigate thermal effects upon the circulators and test section without the use of HX-3 during depressurization transients. In other transients and during low-power steady-state testing, the bypass helium flow through HX-2 can be used to control loop temperatures. Thus HX-3 has been removed from the loop design.

Performance analysis of the loop has been completed for a wide range of steady-state operations and for the depressurization transients. Design information has been provided to the analytical group, and design personnel have participated in review and evaluation of analytical results and in planning analytical effort. This liaison has contributed to the development of preliminary designs for much of the mechanical equipment; the results increase confidence that the selected equipment will meet functional requirements for the CFTL.

A coordinated effort has also been initiated with Y-12 Civil and Architectural Engineering for the design of structures such as the cooling-air filter enclosures at the inlet of the air handling systems, a penthouse enclosure and electrical hoist over the roof opening above the test vessel, and a parking pad for the helium supply trailer. The scope of work in support of mechanical equipment has been identified, and requirements have been established. It is estimated that this work will be completed by the middle of CY 1978.

1.3.2. Electrical Design - C. F. Holloway

The electrical engineering portion of the cost estimate for constructing the CFTL loop was made, based on the advanced conceptual design discussed herein. Advanced conceptual design of the electric power distribution system for the CFTL was performed in CY 1977. Preliminary specifications for the major components which in general conform to industry standards were prepared. These included metal-clad switchgear with 13.8-kV air circuit breaker, 2500-kVA power transformer with associated 480-V switchgear, 1000-kVA power transformer with associated

480-V switchgear, 750-kVA power transformer with associated 480-V switchgear, 480-V motor control center, and 480-V uninterruptible power supply system.

Preliminary design drawings and specifications for the major equipment and cabinets that compose the test bundle power supply and control were prepared for planning and for packaging techniques, to provide a useful and advantageous equipment arrangement. These included induction voltage regulator, multipower level transformer with SCRs for power output control, circuit breaker and fuse cabinet, crowbar SCR cabinet, current transformer cabinet, patch panel and current transformer cabinet, potential transformer and transducer cabinet, crowbar relay cabinet, crowbar detector cabinet, and crowbar pulse transformer cabinet.

One-line electrical power diagrams, electrical schematic and block diagrams of equipment, and layouts for the equipment were made. These drawings together with the major equipment specifications, design drawings, and a material listing served as a basis for the electrical engineering portion of the CFTL construction cost estimate.

The source of electrical power for the CFTL is a 161-kV, three-phase, 60-Hz line from the Elza substation (located within 1 mile of Building 9201-3, the site of the CFTL) to a 40-MVA, 161- to 13.8-kV transformer located outside at the southeast corner of the site building, which supplies power to the 13.8-kV switchgear located inside the building at the southeast corner of the second floor. The 40-MVA transformer has adequate reserve capacity for the CFTL requirements. A one-line diagram of the power distribution system is shown in Fig. 1.34.

The design requires the addition of one 13.8-kV switchgear section with an air circuit breaker for the source of power for the 2500-, 1000-, and 750-kVA transformers. Design specifications for the 13.8-kV switchgear section and the power transformers and associated 480-V switchgear have been issued for comment. The overall design for this equipment is about 40% complete. The planned ventilated dry-type transformers have provisions for the future addition of fans to provide supplementary increased kVA ratings. The primary voltage rating is 13.8 kV and the secondary is wye connected with a voltage rating of 277/480 V. Standard full-capacity taps of two plus 2.5% and two minus 2.5% will be provided.

Fig. 1.34. Power Distrit

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Fig. 1.34. Power Distribution System — A One-Line Diagram.

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Summary

tion System - A One-Line Diagram.

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These taps may be hand operated outside the case when the transformer is not energized. The 480-V switchgear is to be located adjacent to its respective transformer to form a unit-type substation, and these stations are to be located inside the building on the first floor near the experimental equipment.

Specifications for the helium circulator variable-frequency power supply were completed, and one of the units was ordered and delivered.

The test bundle power supply and control consists of 13 variable-voltage zone power supplies, with each zone divided into approximately 10 circuits to form a total of 131 circuits, which lead to a patch panel. Each of these circuits is provided with crowbar overcurrent and differential current protection for limiting the current to a faulty fuel rod simulator. From the patch panel, 91 circuits supply power to the tube bundle rods. Each circuit has power-measuring equipment and balance resistors for equalizing the power to the fuel rod simulators. A block diagram and typical schematic of this equipment is shown in Fig. 1.35.

The power supply and test bundle power equipment layouts are shown in Figs. 1.36 and 1.37 respectively.

1.3.3 Instrumentation and Controls Design - P. G. Herndon

The advanced conceptual design of instrumentation and control systems for the CFTL was completed. The instrument application diagrams shown in Figs. 1.38 and 1.39 define the systems for the circulating loop and a test bundle installed in the test vessel. A tabulation of all components shown on the diagrams and a preliminary set of specification sheets for these components were also completed. The design information served as the basis for preparing an estimate of the cost of designing and building the instrumentation and control systems for the CFTL; it also served as the basis for formulating work plans for FY-1978 design and construction activities. These plans included a work breakdown structure, an E-Z PERT planning chart, and manpower use schedules.

The conceptual design will also serve as a guide during FY 1978 for developing detailed instrument application diagrams, electrical control circuit elementary diagrams, and control panel layouts.

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SCHEMATIC DIAGRAM-TYPICAL ROD CIRCUIT

1st Bundle Power Block Diagram.

Fig. 1.36. Power Supply Equipment Layout.

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Fig. 1.37. Test Bundle Power Equipment

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Fig. 1.37. Test Bundle Power Equipment Layout.

Fig. 1.38. GCFR-Core Flow Test Loop

Low Test Loop Instrument Application Diagram.

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Fig. 1.39. GCFR-Core Flow Test Loop Test Section Instrument Application Diagram.

1.3.3.1 Control Room Plan

The conceptual control room plan in Fig. 1.40 shows the layout of the measurement signal conditioning cabinets, the operating instrument cabinets, and the data acquisition equipment. The plan establishes control room space as well as utility and fire-protection requirements; it was approved by the Y-12 Plant Fire Protection Engineering Department. The plan along with equipment size and weight information also provides the structural and architectural engineers with information needed to make a preliminary analysis of the loading on the building (9201-3) structure.

1.3.3.2 Process Instrumentation

Preliminary design information was provided to aid the mechanical engineering designers of piping and vessels as follows:

1. The number, size, and type of signal conductors connected to the measurement sensors located inside the test vessel were estimated. This information was needed to provide a basis for the design of the test vessel closure because all conductors must penetrate the closure through high-temperature, high-pressure fittings.

2. A typical design, shown in Fig. 1.41, was recommended for making a transition connection between the 0.020-in.-diam thermocouple assemblies in individual rods and their associated extension lead wires. This information made it possible for the test vessel designer to provide adequate space for the approximately 500 signal conductors that are concentrated directly above the closure at the top end of the test vessel.

3. Sketches showing overall dimensions and weights of typical valves and flow tube assemblies proposed for the CFTL were provided to aid in the conceptual design of the main helium loop piping and supports.

1.3.3.3 Helium Circulator Instrumentation - P. G. Herndon and W. R. Miller

Mechanical Technology, Inc., manufacturers of the CFTL helium circulators, established the following two requirements for protecting

Fig. 1.40. GCFR-Core Flow Test Loop Control Room Plan.

Fig. 1.41. FRS Thermocouple to Extension Lead Wire Splice.

the circulator gas bearings from excessive wear and reverse rotation during start-up:

1. on start-up, accelerate the rotor assembly from 0 to 3000 rpm in 1 s; and
2. if reverse rotation is detected, shut off the electrical power to the drive motor within 0.1 to 0.2 s after start-up.

The first requirement is provided by the variable-frequency power supply control system. To meet the second requirement, W. R. Miller of ORNL Instrumentation and Controls Division designed, assembled, and tested a prototype of the rotational speed and direction detection system. The system, shown in the function block diagram of Fig. 1.42, consists of a 120-tooth martensitic stainless steel gear that is mounted on the circulator shaft for signal generation, two magnetic sensors, one electronic signal conditioning chassis, and interconnecting cables.

As the gear on the circulator shaft rotates, the magnetic gear tooth sensors 1 and 2 generate sine-wave ac voltages that are 90° out of phase because of the position of the sensors in relation to the gear teeth. The voltages are converted by the signal conditioners to pulse train outputs whose frequency is proportional to the speed of rotation. Each pulse train can be used to drive a pulse counter, frequency-to-dc converting devices, or the DAS input to obtain an indication of speed in rpm.

Fig. 1.42. Circulator Speed and Rotational Direction Detector.

The two pulse signals are also fed to the direction computer, which provides a steady-state direct-current (dc) output of "0" Vdc, or +5 Vdc in accordance with the phase relationship of the pulses received. The phase relationship of the pulses is determined by the rotation direction of the gear teeth past the sensor heads. The steady-state dc (or direction) output is used to disconnect the circulator motor from the power supply if reverse rotation is indicated. In addition to rotational speed and direction indication, the system also provides a feedback signal to the motor power supply that controls the ramp rate during startup.

The Servo-Optics Systems of California conducted performance tests on the power supply they built for the CFTL by loading it with a prototype of the circulator drive motor. They installed and used the ORNL prototype speed and rotational direction-detection system during these tests, and it performed satisfactorily.

Twenty-four 1/16-in.-OD, stainless-steel-sheathed thermocouple assemblies were designed and fabricated. The thermocouples will be embedded in the circulator motor windings. Six of these assemblies were

shipped to TASK, the Mechanical Technology, Inc. subcontractor responsible for constructing the motors for the circulator prototype.

Construction drawings of the helium circulator test loop submitted by MTI were reviewed and approved for construction.

1.3.4 Dynamics and Control Analysis - S. J. Ball

The CFTL dynamics simulator code ORCULT was updated from its original preliminary loop design version and now corresponds to the present advanced conceptual design. In the process of developing the present simulator, several analyses were made that helped to solve loop layout and component design problems.

The first question addressed was that of an optimum heat exchanger configuration. In the conceptual design a 400-kW heat exchanger (HX-3) was installed in parallel with the main 4-MW exchanger (HX-1); there was uncertainty about the controllability of HX-1 at very low helium-flow and heat-rejection rates. The results of simulations of critical transients demonstrated that HX-3 could be eliminated if one took advantage of the long temperature response times inherent in low-flow operation and used a wide range of cooling-air flow rates for HX-1. Elimination of HX-3 will remove a considerable amount of loop hardware and will also simplify loop operation and control problems.

Several approaches to controlling the HX-1 flow over wide ranges have been investigated. The cooling-air flow path through HX-1 was revised to the "upflow" direction to provide for a smooth transition from forced to natural convection flow. A simulation of the natural convection flow was developed. Results indicated that up to 800 kW can be removed from HX-1 by natural convection alone. The present plans for coolant control call for the use of variable blower speeds and position of inlet vanes with forced convection flow and the use of variable louver (or damper) positions for natural convection flow. For the zero and near-zero test bundle power cases, cooling-air preheaters may be used, possibly in conjunction with a smaller control valve in the air duct to obtain finer control of the low natural convection flows.

A control system for the CFTL heat exchangers at higher powers was developed and checked out using the simulator. The system uses a

feedback signal from the heat exchanger helium-outlet temperature in conjunction with feed-forward signals from the helium-inlet temperature and flow rate to vary coolant air flow. The performance of the system has been satisfactory on runs made to date if it is assumed that the air flow can actually be controlled as demanded. The effects of "imperfect" air flow control are being investigated.

Because of the wide variety of demands on the ORCULT code, it was converted into the following several different special-purpose codes, all of which are now operational and in use:-

1. A loop temperature and flow steady-state code is used to establish steady-state circulator speeds, valve coefficients, cooling air flows and outlet temperatures, and loop heat-loss parameters for specified cases of test section power, flow, and inlet temperature, attenuator flow, and bypass flow.
2. A loop temperature and flow dynamics code was established for cases that do not involve depressurization but which do include the bypass heat exchanger (HX-2) loop.
3. A second loop temperature and flow dynamics code includes depressurization capability (helium conservation of mass equations) but omits HX-2. (Present loop operating plans call for the valving off of HX-2 during depressurization.)
4. Simplified, hydraulics-only codes for both the depressurization and nondepressurization tests were written to perform scoping studies of valve and circulator speed-control system designs.

Studies were made of a representative set of steady-state loop operating conditions corresponding to the September 1977 version of GA's Total CFTL Test Specifications. This set of conditions covers the extreme ranges of loop power, temperature, and flow. Of particular interest were the control valve rangeability (C_v) requirements, cooling-air flow and auxiliary heating requirements for the heat exchangers, and circulator speed control requirements.

Control of heat exchanger cooling rates, control valve manipulations, and the resulting temperature and flow transients during design basis depressurization accident (DBDA) simulations were also investigated for a variety of cases. It was found that the predicted circulator and test section inlet temperature perturbations can be held to

very small values during the transients. Also, the control scheme proposed for valve manipulations achieves the desired test section flow transients and keeps the circulator well within its desired operating range. Simulations to date indicate that loop piping and heat-exchanger temperatures can be maintained within design limits while simulating DBDA leak sizes as large as 0.064 m^2 (100 in.²) for initial test bundle powers up to 30.8 kW per rod. The reactor scram power transient used in the simulation corresponds directly to the predicted GCFR scram curve, and a nuclear fuel rod, not a fuel rod simulator thermal model, is used in ORCULT for these runs. For actual loop operation, modified power transients must be used with the fuel rod simulator to give the equivalent clad and helium outlet temperature trajectories. The calculation of an estimated fuel rod simulator power transient can be done with a code developed by R. A. Hedrick.⁷

Other special-purpose simulation runs were also made. One run was made to obtain estimates of the maximum radial temperature gradients expected in the 6-in., schedule-80 loop piping to calculate thermal stresses. It was assumed that the maximum gradient would occur just downstream of the test section for an inadvertent loss of test section power with full helium flow and pressure maintained. The resulting maximum gradient was $45^\circ\text{C}/\text{cm}$ ($204^\circ\text{F}/\text{in.}$) or 49°C (88°F) across the 1.10-cm-pipe (0.432-in.) wall. This was judged to result in acceptable stress levels.

A preliminary study made of several casualties leading to a complete and sudden loss of HX-1 cooling indicated the need to scram power to both the test bundle and the circulators to preserve both from the deleterious effects of overheating.

1.4 ANALYTICAL ACTIVITIES

1.4.1 Thermal-Flow Analysis of the Test Bundle — J. P. Sanders and J. C. Conklin

The temperatures of the fuel rod simulators must be accurately predicted for all test conditions to prevent unanticipated failure. A number of subchannel-analysis computer codes have been developed to calculate the thermal-flow characteristics of rod bundles. The COBRA

series of codes was chosen by GA for the GCFR and will be used for the CFTL. The present version,² CØBRA IIIC, cannot be used if there is a significant diversion of flow among the subchannels. A nodal-point code,³ FLAC, written by GA, will be used for the inlet region of the bundle in which the cross flow is approximately equal to the axial flow. These codes were requested from GA early in the year and received about the first week of July.

CØBRA IIIC was originally written for a water-cooled rod bundle. A special modification was developed by Battelle Northwest Laboratories for GA to represent the flow of high-pressure helium in a rod bundle. This modification removed the water-related subroutines and statements and replaced them with the appropriate statements representing helium. General Atomics further modified the code for use on their computer system.

Two associated programs for use with CØBRA IIIC were also received, SPECSET and GEOM. SPECSET sizes the arrays and equivalence statements for any specified bundle dimensions and symmetry. GEOM determines the geometric factors needed to describe the various flow channels from a general description of the bundle and punches this information on cards in the input format required by CØBRA.

General Atomics uses a Univac 1110 computer system with a FØRTRAN V compiler. Both codes had been optimized to use features that are not available with the IBM system at ORNL, which has a FØRTRAN IV compiler and, therefore, had to be carefully modified to achieve analogous computations. The most significant incompatibility is that the Univac compiler allows the use of variables as array dimensions with the desired value specified at the compilation, a powerful feature that is not available on the IBM machine. A processor program was written for FLAC, which treats the variables as character strings and replaces the array dimension variables with the appropriate integers.

The array-dimensioning scheme for CØBRA IIIC was considerably more complicated than that for FLAC and, consequently, required a more sophisticated method for adaptation. Special job control language (JCL) routines were written that first execute SPECSET, then use IBM direct access device utility programs to perform the manipulations of the output

which the Univac FORTRAN V compiler would have performed, and finally prepare an IBM statement listing that is compatible with the FORTRAN IV compiler.

Implementation of the COBRA IIIC, as modified for helium, has been completed. The sample output that CA initially furnished to ORNL with the COBRA coding failed to agree with the ORNL output in the second and subsequent significant digits for values resulting from the iterative solution method, a discrepancy due to roundoff errors when the IBM single precision, four-byte word is used. Therefore, the length of all floating-point numbers was increased to eight bytes. This change required all the COMMON blocks to be reordered to avoid addressing errors, and suitable modification of the subroutines and special functions were made to account for the double-word length, which was an extensive and time-consuming task, but necessary. The sample output from CA (Univac) now agrees exactly with that produced by ORNL (IBM) when using the same sample input.

The FLAC code is also now operational on the ORNL IBM system. Of the three solution methods used in the code, the implicit-solution technique had repeatedly failed to converge for properly prepared input for the 37-rod bundle. The principal-path method was not applicable to the solution of this problem. The direct-solution method was then expanded by increasing the array sizes of the appropriate subroutines as necessary, which is now done automatically by the processor program. This method has been successfully used for a model of 124 nodes and 263 branches representing a 60° symmetrical segment of the inlet section of the 37-rod bundle. The additional storage requirements for the modified version of FLAC do not appear to approach the limit of available computer storage at ORNL for any of the foreseen modeling requirements.

Extensive effort has been expended to adapt this general network code, originally written for a Univac machine, to the specific case of the CFIL bundle entrance region, using an IBM machine. Considerable effort is also necessary to prepare the input. Figure 1.43 is an example of a ten-axial-node flow network that must be generated by hand. Each individual branch characteristic must be specified and discretely entered as input to FLAC. Figure 1.44 represents the inlet flow distribution for the 37-rod bundle channels as designated in Fig. 1.45 for a ten-axial-

Fig. 1.43. Nodal Point Network for 37-Rod Inlet Region with Ten Axial Nodes.

Fig. 1.44. Normalized Flow vs Axial Position for the Inlet Section of a 37-Rod Test Bundle.

Fig. 1.45. One-Sixth Symmetrical Segment of a 37-Rod Test Bundle.

node model. The lower flow in the edge channels is due to the additional friction of the duct wall and large-diameter hanger rods. Figure 1.44 was generated using a 0.5-velocity head as a loss coefficient for cross flow between fuel rod simulators. The pressure drop over the first 254.0 mm (10.0 in.) was calculated to be 19.3 kPa (2.8 psi). Use of a 1.0-velocity-head cross-flow loss resulted in the development of the flow distribution occurring 25 mm further downstream and a pressure drop of 20.86 kPa (3.025 psi). The actual loss coefficient should be between 0.5 and 1.0 kPa.

1.4.2 Structural Analysis of the Test Bundle -- W. G. Dodge

During the past year, ORNL initiated activities dealing with structural aspects of the CFTL test bundles. The emphasis of this work is on evaluating the thermal- and flow-induced structural response that results in progressive deterioration of the test bundles. The primary purpose of this effort is to ensure that the CFTL test program will produce structural data that are meaningful in the evaluation of test bundle performance and in the validation of analytical techniques that will be used subsequently for the evaluation of the GCFR core assemblies.

Structural analysis of the CFTL bundles will also be necessary to provide pretest predictions of response and to assist with data reduction during operation to avoid the unanticipated failure of the test bundle.

A study was made to determine what structural measurements appear feasible, using currently available instrumentation. The results of this work indicate that, because of constraints imposed by the high temperatures and space limitations, the experimental data available for evaluation of heater rod and spacer grid structural response will be very limited; however, it appears feasible to determine experimentally hanger rod loads and duct deformations. Thus it will be necessary to rely on analytical techniques to elucidate structural behavior of test bundles from limited experimental observations.

Various computational procedures were reviewed to ascertain their applicability for determining the structural response of the CFTL bundles. Two candidates that appear to offer the greatest potential were identified as the displacement-based finite element method and the transfer matrix method.

1.4.2.1 The Finite Element Method

The displacement-based finite element method has been demonstrated to be effective for solving a wide variety of linear and nonlinear structural problems, and it is natural to consider application of the technique to the analysis of the CFTL bundles. In the finite element method, the structure is reduced to a finite number of discrete structural elements, and appropriate force-displacement relationships are developed for each element. These relationships take the form

$$[K]_i \{\delta\}_i = \{F\}_i, \quad (1.1)$$

where $\{\delta\}_i$ is a column vector of the nodal displacement degrees-of-freedom of the element, $\{F\}_i$ is a column of the nodal forces corresponding to the nodal displacement, and $[K]_i$ is a square matrix called the stiffness matrix for the element. The force-displacement relationships for the total structure are obtained by combining the element stiffness equations,

recognizing that several elements may share the same nodal degrees-of-freedom and forces. The latter process is called assemblage and leads to a system of equations of the form

$$[K]\{u\} = \{F\}, \quad (1.2)$$

where $\{u\}$ is a column vector containing all the nodal degrees-of-freedom of the structure, $\{F\}$ is a column vector containing the external nodal forces and pseudonodal forces due to temperature, etc., and $[K]$ is a banded diagonal matrix, called the global stiffness matrix, which contains the combined element stiffnesses.

Two problems arise when one attempts to apply the finite element technique to structures such as the CFTL bundles. The first problem is the large amount of computation necessary to assemble and solve the set of stiffness equations for the complete structure. For a structure like the CFTL bundles, this will be a large number of equations (i.e., ~6000 equations, with a diagonal bandwidth of ~1000). The second problem arises from the simultaneous inclusion of very stiff and very flexible elements in the global stiffness matrix, a situation that could lead to poor numerical conditioning for the set of equations. Thus direct application of the finite element method requires solving a large system of equations that are likely to be poorly conditioned numerically.

Both of these problems can be reduced in magnitude by assuming that the spacer grids are infinitely stiff. With this assumption, the same lateral degrees-of-freedom can be assigned to all bundle fuel rod simulators and hanger rods at each longitudinal section containing a spacer grid. Thus the number of equations and the average bandwidth are reduced by about 40%, which results in an approximately 80% reduction in the computational effort required to solve the system of equations. The elimination of the large stiffness terms associated with the deformation of the spacer grids will result in improved numerical conditioning for the system of equations. The implementation of this approximation into most modern finite element codes should require only minor modification of the algorithm that assigns equation numbers to the nodal degrees-of-freedom.

1.4.2.2 The Transfer Matrix Method

An alternate computational procedure considered is an uncoupled iterative procedure based on the transfer matrix technique. The relationship between the transfer matrix method and the finite element method can be seen by considering the simple case of the bending of a straight cantilever beam.

Suppose the beam is composed of six beam elements joined end-to-end at nodes as shown in Fig. 1.46. The stiffness matrix for the element i which connects nodes i and $i + 1$ takes the form

$$[K_L] \begin{Bmatrix} u_i \\ \theta_i \\ u_{i+1} \\ \theta_{i+1} \end{Bmatrix} = \begin{Bmatrix} V_i \\ M_i \\ V_{i+1} \\ M_{i+1} \end{Bmatrix}, \quad (1.3)$$

where u_i and θ_i are, respectively, the transverse displacement and rotation at node i , and V_i and M_i are the transverse shear and internal bending moment at the end of the beam segment adjacent to node i . In the finite element method, the individual element stiffnesses are assembled into a set of 14 equations,

$$[K] \begin{Bmatrix} u_1 \\ \theta_1 \\ u_2 \\ \vdots \\ \vdots \\ \vdots \\ \theta_6 \\ u_7 \\ \theta_7 \end{Bmatrix} = \begin{Bmatrix} P \\ -P_1 \\ 0 \\ \vdots \\ \vdots \\ \vdots \\ 0 \\ -P' \\ 0 \end{Bmatrix}, \quad (1.4)$$

which are then solved. (Note: u_1 and θ_1 are identically zero.)

Fig. 1.46. Example of a Cantilever Beam Composed of Six Beam Segments.

The transfer matrix for beam element i takes the form

$$\begin{Bmatrix} u_{i+1} \\ \theta_{i+1} \\ v_{i+1} \\ M_{i+1} \end{Bmatrix} = [T_i] \begin{Bmatrix} u_i \\ \theta_i \\ v_i \\ M_i \end{Bmatrix} \quad (1.5)$$

and thus differs from that of the stiffness matrix in that all of the parameters associated with nodes i and $i + 1$ are collected into separate vectors called the state vectors for nodes i and $i + 1$. Since the state vector at the first node is known, that is,

$$\begin{Bmatrix} u_1 \\ \theta_1 \\ v_1 \\ M_1 \end{Bmatrix} = \begin{Bmatrix} 0 \\ 0 \\ P \\ -Pl \end{Bmatrix}, \quad (1.6)$$

the state vector at the second node can be obtained by

$$\begin{Bmatrix} u_2 \\ \theta_2 \\ v_2 \\ M_2 \end{Bmatrix} = [T_1] \begin{Bmatrix} 0 \\ 0 \\ P \\ -Pl \end{Bmatrix} \quad (1.7)$$

The state vector at the seventh node is given by

$$\begin{Bmatrix} u_7 \\ \theta_7 \\ V_7 \\ H_7 \end{Bmatrix} = [T_7] [T_6] \dots [T_1] \begin{Bmatrix} 0 \\ 0 \\ P \\ -Pl \end{Bmatrix} \quad (1.8)$$

Solution of the problem, using the transfer matrix method, requires only successive multiplication of the 4×4 transfer matrices rather than the assemblage and simultaneous solution of 14 equations, as required by the finite element method.

Thus both the finite element method and the transfer matrix method use the same elemental models but differ in the manner in which the computations are performed. Because of this difference, highly statically indeterminate structures can be analyzed quite easily using the finite element method, but the transfer matrix method is most suitable for structures that are statically determinate. Simple cases of static indeterminacy can be handled within the context of the transfer matrix method by using specialized techniques; however, specialized computer programs are generally required for each new application. This lack of generality in the transfer matrix method is the primary reason it is not widely used.

In applying the transfer matrix method to a structure, two items require consideration: the determination of appropriate transfer matrices for the basic structural elements and the development of appropriate algorithms to account for the interaction between various portions of the structure or between the structure and its environment. The basic structural element of a CPTL bundle is a straight beam subjected to bending and torsional moments, transfer shearing forces, axial forces, and thermal loadings; the development of this matrix is straightforward. To simplify the computations required in applying the transfer matrix technique to the CPTL bundles, it is proposed that the computations be separated into two distinct parts, with the interaction being accounted for by iteration on the variables defining the interaction.

The first portion of the computation involves using the transfer matrix to determine the deflections of the hanger rods and ducts under the action of the thermal loading and previously determined spacer grid loading. In this calculation, the interaction between the ducts and hanger rods is accounted for by introducing a point transfer matrix where the interaction is represented by appropriate spring elements. After determining the deflections of the tie rods and ducts using the above procedure, the displacements of the contact points between the spacer grids and rods can be determined by assuming that the spacer grids are rigid. The deformations and contact forces for each rod can then be determined by applying the transfer matrix procedure to each rod individually. In this calculation, special point matrices are required to account for the gap clearances between the rods and the spacer grid. Once the contact forces are determined, the net reaction of the spacer grid on the tie rods can be determined and an updated deformation for the tie rods and duct obtained. These values are then used to determine updated values for the spacer grid displacements and hence the rods. Iteration continues at this level until satisfactory convergence is obtained.

1.4.2.3 Conclusions

Of the two procedures outlined, the transfer matrix method appears to be the more complex and requires iteration at two levels; however, because of the dominance of the duct and hanger rod stiffness, convergence should be quite fast. The procedure requires only the manipulation of small transfer matrices rather than the assemblage and solution of the very large stiffness matrices required by the more conventional finite element method. Thus the transfer matrix method appears to be more economical from a computational standpoint, but the finite element method is more conventional and, consequently, will not require as much development. A meeting between ORNL and General Atomics personnel will be held early in 1978 to decide on which method ORNL will use for structural analysis of the CFTL assemblies.

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2. GCFR SHIELDING STUDIES

D. E. Bartine

2.1 INTRODUCTION

This report covers work performed from January through December 1977. The primary emphasis was on the design, preanalysis, and fabrication of the Tower Shielding Facility (TSF) experiment for the investigation of neutron streaming in a GCFR-type lattice and the effectiveness of the grid plate shield design. Extensive calculations performed concurrent to the assembly design were required to construct a small-scale experiment that was highly prototypic of large-scale GCFR design. Measurements were begun in October and will continue through the first quarter of 1978. Other accomplishments included (1) analysis of the GCFR core grid plate damage, (2) analysis of the radial and the upper and lower axial shield designs with a new streaming source, and (3) analysis of advanced core designs for alternate fuel cycle considerations. Finally, a cooperative program between ORNL and the Swiss Federal Institute for Reactor Development (EIR), Wurenlingen, Switzerland, was established whereby ORNL will perform some preanalysis and postanalysis of GCFR reactor physics and shielding experiments to be conducted on the Proteus facility at EIR.

2.2 GRID PLATE SHIELD DESIGN CONFIRMATION EXPERIMENT - D. T. Ingersoll, F. J. Muckenthaler, C. O. Slater, and M. L. Williams

2.2.1 Design Fabrication

The GCFR experimental measurements to provide confirmation of calculations for the grid plate shield design study have been divided into four basic parts, which are (1) measurement of the TSR-II (Tower Shielding Reactor with 30-in. beam opening) neutron flux distribution from inside the void where the GCFR fuel pins will be located, (2) measurement of the neutron source term incident on the grid plate shield, (3) mapping of the neutron flux emerging from the grid plate shield in search of possible neutron streaming between subassemblies and measurement of the flux incident on the grid plate, and (4) determination of a

source term for the upper axial shield through measurements following the prototypic grid plate.

The data plan was divided into five categories, with the highest priority (1A) being given to those measurements for which the steel-lined concrete configuration contained seven subassemblies each containing 127 fuel pins and spaced 12.7 mm apart. For the second priority (1B) configurations, the only alteration is the spacing between subassemblies, which will be changed to 6.35 mm. In priority 1C, the subassemblies will contain 91 pins each, and the spacing between subassemblies will be 12.7 mm. Should time permit, measurements for priorities 2 and 3 will be completed. For priority 2, the 19 pins in the middle of the central fuel subassembly (priority 1A) will be removed to simulate a control rod assembly. The configuration for the last priority measurements puts only a 3.175-mm spacing between fuel pin subassemblies. Priority 1A was completed during the first quarter of FY 1978.

One of the objectives of this experiment was to establish a neutron source term for axial streaming, as close as feasible to that predicted for the GCFR core. This could be obtained only if the neutron flux coming directly from the TSR-II was "minimized" with respect to the flux coming from sources in the fuel pins. Calculations showed that placement of a 305-mm-thick concrete shadow shield between the reactor and fuel pins, to be surrounded by a 152.4-mm void for the first 610 mm of fuel pin length, would limit the TSR-II contribution to <10% of the neutron flux source emerging from the fuel assembly configuration. This design was incorporated into the experiment and is shown in Fig. 2.1.

The GCFR core is simulated by seven subassemblies containing slightly enriched UO₂ fuel pins (1.99% ²³⁵U) clad in 0.813-mm-thick aluminum wall tubing having an OD of 12.7 mm. Each pin contains 1035.49 g of uranium fabricated in small pellets 10.95 mm in diam, placed end on to give a length of 1219.2 mm. Aluminum studs on each end complete the 1270-mm-long fuel pin.

The B₄C subassemblies were hexagonal in shape to match the fuel pin subassemblies. The inside liner (made from 7.94-mm-thick iron plate) was also hexagonal but was tapered along the axis to provide an 11.0-mm

Fig. 2.1. Initial Configuration, Which Includes Only the Concrete Shadow Shield and Surrounding Reflector.

thickness of B_4C next to the fuel pins, expanding to a 31.1-mm thickness near the other end. The B_4C was vibra-packed to give density values of 76.2 to 77.2% theoretical. The actual length of B_4C was 323.1 mm, with the remaining 127 mm consisting of iron at the ends of the B_4C .

The grid plate consisted of a 635-mm-thick stack of iron plates containing seven cylindrical holes 165.1 mm in diam, whose axes were aligned with the axes of the fuel pin and B_4C subassemblies for priority 1A. Removable sleeves, containing B_4C , were fabricated to reduce the cylindrical void to about 120.7 mm through the first 228.6 mm of the grid plate.

The first series of measurements were made to check the calculation of the flux distribution from the TSF reactor behind the concrete shadow shield prior to inserting the fuel pins. The flux distribution along the axis of the cavity to be filled with the fuel pins was mapped using the Hornyak button and both bare, cadmium-covered and 5-in. Bonner balls. Radial measurements were also made with the same detectors at 300 mm behind the concrete shield containing the cavity.

The fuel pin subassemblies, containing 127 pins each, were then placed into the cavity, leaving a separation of 12.7 mm between the subassemblies. This configuration is shown in Fig. 2.2. Measurements beyond the fuel pins include both a neutron spectrum of the emergent flux on the centerline and radial traverses with the cadmium-covered and 5-in. Bonner balls and the Hornyak button at both 305 mm and 3048 mm beyond the fuel pins. These traverses were, as were all the radial traverses in this experiment, in the horizontal plane where the detector passes through the axes of three subassemblies (Fig. 2.2). The measurements provide a comparison with calculations of the source term incident on the grid plate shield. A photograph of the subassembly is given in Fig. 2.3.

The B_4C subassemblies forming the grid plate shield were then placed behind the fuel pins (Fig. 2.4). Spacing between the B_4C assemblies was also 12.7 mm and the axes of the B_4C pieces were coincident with those of the fuel pins. Radial traverses were made adjacent to the ends of the B_4C subassemblies with the cadmium-covered Bonner ball and the 6.35-mm-diam (1.59-mm-thick) Hornyak button. Results of

Fig. 2.2. Second Configuration, Which Includes the Addition of Seven Hexagonal Fuel Pin Assemblies.

these mappings are given in Fig. 2.5, which clearly indicate significant streaming between subassemblies. As expected, the magnitude of the streaming through the void in the outside assemblies is greater than through the center assembly because the source term is greater along the outer edge of the fuel pin assembly. Measurements were made along the centerline at 305 mm and 3048 mm beyond the grid plate shield using the cadmium-covered and 5-in. Bonner balls and the Hornyak button. Axial traverses through the void in the center B_4C subassembly were made from the fuel pins to 305 mm beyond the end of the grid plate shield.

Fig. 2.5. Radial Traverses Behind the Third Configuration (Fig. 2.4) Using the 0.25-in. Hornyak Button and the Cadmium-Covered Bonner Ball Detectors.

A series of iron slabs (610 mm thick) were placed 152.4 mm behind the B_4C shield (Fig. 2.6), and the cadmium-covered and 5-in. Bonner ball measurements were repeated in the void. The iron was introduced because its back-scattering component, which is readily calculated, reduces the sharp gradient in the neutron flux that occurs in moving from a highly attenuating medium to a much less dense medium such as air. Comparisons between calculations and measurements are therefore much more meaningful.

Fig. 2.6. Fourth Configuration, Which Includes a 24-in. Reflecting Iron Slab Behind the Detector Positions.

The configurations for the first series of measurements (priority 1A) were completed with the addition of the prototypic grid plate directly behind the grid plate shield as shown in Fig. 2.7. Measurements were made along the centerline with the neutron spectrometers (NE-213 scintillator and hydrogen counters) and Bonner balls. Radial traverses were made directly behind the grid plate with the Hornyak button and at 305 mm and 3048 mm with the cadmium-covered and the 5-in. Bonner balls and the Hornyak button. Radial traverses with the Hornyak button and the 5-in. Bonner ball were also repeated at 305 mm without the B_4C inserts in the grid plate indicated in Fig. 2.7. Results from the radial traverses at 305 mm behind the grid plate are plotted in Fig. 2.8. The Hornyak button data indicated flux peaks occurring back of the iron between the cylindrical voids. These peaks are a result of the scattered contributions from the grid plate holes directly above and below the iron that separates the holes in the plane of the radial traverse. These small peaks disappeared when a similar traverse was made directly (~ 25.4 mm) behind the grid plate. The degree of collimation that the voids in the B_4C subassemblies and the cylindrical hole in the grid plate provide is

seen by the plot of the Hornyak button and cadmium-covered Bonner ball traverses at 3048 mm as shown in Fig. 2.9.

Fig. 2.9. Radial Traverses at 305 cm Behind the Prototypic Grid Plate.

2.2.2 Calculations in Support of Experimental Design

Basic neutron transport calculations, directed toward the experiment design, were begun in January 1977. The first calculations were based on the 300-MW(e) GCFR design and were performed to investigate the effects on design flux levels of modeling only seven subassemblies, both with and without an outer reflector. It was concluded that the absence of a reflector would produce a significantly harder neutron spectrum at the grid plate and that, even with a reflector, some difficulty could be anticipated in designing an experiment that could represent a semi-infinite fuel pin array.

With the cooperation of the General Atomic Company (GA), a preliminary design for the experiment was established and is shown in Fig. 2.10. General agreement was obtained that the experiment should be as prototypic of GCFR design as possible. Several test requirements

Fig. 2.10: Initial Conceptual Design of the Grid Plate Shield Design Confirmation Experiment.

were established to guide the detailed design of the experiment; foremost was that the predominant source of neutrons originate within the fuel pins. Preliminary calculations showed that, with the experiment model and GCFR-type fuel pins, the leakage from the fuel assemblies was dominated by neutrons from fission within the pins rather than neutrons arriving directly from the TSF reactor beam. Later calculations with only slightly enriched UO_2 fuel pins showed that the fission source was still dominant.

The early scoping calculations were performed, using primarily the DOT-IV discrete-ordinates code.¹ The cross sections and source data were collapsed to 7 energy groups from a 51-group GCFR library. The adequacy of the 7-group cross sections was investigated by performing 7-group and 51-group ANISN calculations² of a fission source transported

through laminated slabs of steel and concrete. Although the 7-group calculations showed some spectral distortion, the total flux differed from the 51-group results by only 3 to 5%. Similar results were observed when the final experiment design was analyzed with both the 7-group and the 51-group cross sections.

Concern about the uncertainty of the concrete water content prompted an investigation of its effects on thermal neutron albedo and hence fission production in the fuel pins. Calculations showed that, for an 80% increase in the water content of the concrete, the neutron leakage from the fuel pin region decreased by only 11%. Because only very small changes in the water content were expected for the duration of the experiment, it was concluded that the variations would have a negligible effect on the fission source.

The radial fission distribution for the fuel pin assemblies was investigated and found to be significantly peaked at the outside edge of the assemblies. The distribution was sensitive to the thickness of the steel liner that surrounded the assemblies and concrete and became radially flatter as the liner thickness increased. This effect was due to the absorption in the liner of thermal neutrons returning from the concrete. These neutrons would have induced fissions in the outer rows of fuel pins. With the liner thickness chosen (9.5 mm), the fission distribution was still peaked; however, the radial flux distribution impinging on the grid plate shield was relatively flat. This was apparently due to a redistribution of the neutron population through scattering collisions.

Because the experiment was primarily for the confirmation of the grid plate shield design, that shield region was thoroughly investigated. Of some concern was the fact that the experiment grid plate shield sections contained a 26% larger B_4C volume than did the GCFR sections. This was caused by the use of a thinner steel can of equal outside dimensions and the elimination of the internal steel corner posts found in the GCFR design. Offsetting the greater volume, however, was a reduced B_4C density for the experiment. The vibra-packed B_4C assemblies attained only 77% of theoretical density (theoretical density = 2.52 kg/liter) compared with the 99% expected for the GCFR. These effects caused the

total amount of B_4C in the lower grid plate shield sections to be nearly identical for the GCFR and the experiment. Replacing the B_4C in the upper grid plate shield sections with steel caused a 40% increase in the flux at the top of the grid plate for both the experiment and GCFR calculations. Based on these results, it was concluded that both the upper and lower grid plate shield sections required prototypic modeling for the experiment.

A final problem was introduced by the need to determine the importance of neutrons that stream in the gap between fuel assemblies. It was decided to make measurements with interassembly gap widths of 3.2, 6.4, and 12.7 mm, using a single steel liner and concrete configuration. However, this produced a variable-width gap between the seven fuel assemblies and the steel liner. It was felt that streaming from this peripheral gap, which would be as large as 15.9 mm for the 3.2-mm interassembly gap case, might dominate measurements made behind the configuration. Investigations of the peripheral gap considered both an open gap and a steel-plugged gap along the full length of the grid plate shield. Also, calculations were performed with and without a solid reflecting grid plate behind the detector positions. Results for the 5-in. Bonner ball detector (approximately a total flux detector) are presented in Fig. 2.11 for the 15.9-mm peripheral gap case and in Fig. 2.12 for the 12.7-mm case. The most noticeable effect is that the addition of the reflecting grid plate increased the counting rate significantly. The detector, which was traversed at 9.5 cm behind the grid plate shield, showed a maximum 7% decrease in the counting rate caused by the plugging of the 15.9-mm peripheral gap and a much smaller variation for the 12.7-mm gap. Although the S_6 angular quadrature used for the calculations tended to underpredict the streaming effect, it was concluded that plugging of the peripheral gap should be important only for the 3.2-mm interassembly gap case.

2.2.3 Preanalysis of Experiment Configuration

One- and two-dimensional neutron transport calculations for the experimental assembly were performed throughout the design phase, both to guide the assembly design as described in the previous section and to ensure that the experiment would provide interpretable data. Concurrently, two-dimensional calculations for the proposed 300-MW(e) GCFR demonstration

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Fig. 2.11. Calculated 5-in. Bonner Ball Counting Rate (cpm/W) for a Radial Traverse 9.5 cm Behind the Grid Plate Shield - 3.2-mm Interassembly Gap. Cases include open and plugged peripheral gap both with and without a solid steel reflecting grid plate (15.2 cm behind the grid plate shield).

ORNL-DWG 78-1643

Fig. 2.12. Calculated 5-in. Bonner Ball Counting Rate (cpm/W) for a Radial Traverse 9.5 cm Behind the Grid Plate Shield - 6.3-mm Interassembly Gap. Cases include open and plugged peripheral gap both with and without a solid steel reflecting grid plate (15.2 cm behind the grid plate shield).

plant were performed and compared with the experiment calculations to determine the degree to which the experiment is prototypic. The two-dimensional calculations were accomplished by use of the DQT-IV discrete-ordinates code.¹ The experiment was modeled in cylindrical geometry, the fuel pin region being represented by concentric rings of void and material. A boundary source for the experimental assembly was calculated from an existing angular neutron flux distribution at the surface of the TSF reactor. Seven neutron energy groups, P_3 scattering, and S_6 angular quadrature were used for most cases; 51 groups and S_{12} quadrature were used for the final design calculations. A comparison of the 51-group and 7-group results showed that the coarse group calculations were sufficiently accurate for determining integral quantities, such as total fission source and total neutron leakage, but were inadequate for predicting energy distribution.

The primary design criterion required that the dominant source of neutrons for the measurements be within the fuel pin region. It was determined that, with respect to the total neutron leakage out of the grid plate shield region, nearly 90% of the neutron leakage resulted from fissions in the fuel pin array. Furthermore, neutrons from the TSF reactor, which scattered into the fuel pins and subsequently scattered into the grid plate shield region, added significantly to the fuel pin source. Within the fuel pin region, most fissions occurred near the concrete shadow shield; however, fissions that occurred near the grid plate shield assemblies were more important in providing a source to the grid plate shield. These effects are compared in Fig. 2.13, which shows the neutron flux and the contribution flux³ distributed over the model of the experimental assembly. The magnitude of the contribution flux at a spatial location is interpreted as the relative amount that neutrons at the location contribute to the observed response (leakage from the grid plate shield).

The radial distribution of the fission source was found to be peaked at the outside edge of the fuel pin array with edge-to-centerline ratios ranging from 1.4 at the reactor end of the fuel pin array to 11.1 near the axial center of the pin array. Although the edge-to-centerline fission source ratio near the grid plate shield was 6.1, the total flux

ORNL-DWG 77-14664

Fig. 2.13. Comparison of Neutron Flux and Contribution Flux Distributions ($E > 30$ keV) for the TSF Experimental Assembly. The observed response for the channel theory plot is uniform neutron leakage from the grid plate shield.

ratio was found to be 0.9. Figure 2.14, which shows isofission source and isoflux contours calculated for the experimental configuration, clearly illustrates the radial distributions. The isoflux plots also demonstrate the neutron streaming effect in the large peripheral void and the void regions in the grid plate shield. Figure 2.15 gives isoflux plots for the flux greater than 1 MeV and for the thermal flux. The fast-flux isoplot shows the void streaming effect, and the thermal flux isoplot shows the thermal neutron attenuation in the B_4C regions and the flux peaking in the concrete.

A second important design criterion was that the neutron energy spectra within the experimental assembly be prototypic of GCFR-type

ORNL-DWG 78-1652

Fig. 2.14. Isofission Source (Left) and Isoflux (Right) Contours Calculated for the TSF Experiment. The fission source displays a steep radial gradient near the center of the pin array, and the total flux appears flat over the entire length of the fuel pins. Contours are plotted with two lines per order of magnitude.

ORNL-DWG 78-1644

Fig. 2.15. Isoflux Plots for Fast (Left) and Thermal (Right) Neutron Flux Calculated for the TSF Experiment. The fast flux shows a streaming effect in the void regions, and the thermal flux shows considerable attenuation through the B₁C regions. Contours are plotted with two lines per order of magnitude.

spectra. Figure 2.16 shows a comparison of calculated spectra for the TSF experiment (with 91 pins per fuel assembly) and the GCFR design (low-pressure-drop, beginning-of-life case). The two comparisons, taken on the axial centerline directly above the grid plate shield and above the grid plate, show that the experiment produced slightly harder neutron spectra. This result was predicted earlier when the analysis of a seven-assembly reflected cell vs an unreflected cell for the GCFR showed that the flux below 1 MeV decreased significantly more than the flux above 1 MeV for the unreflected case. The concrete reflector in the experiment was designed to improve the infinite-lattice approximation of the seven-assembly cell. It was concluded that the spectra within the TSF experiment resembled the GCFR spectra sufficiently to provide relevant and interpretable data.

Finally, the TSF experiment and the GCFR showed similar responses to changes in the B_{LC} density in both the lower and upper grid plate

Fig. 2.16. Comparison of Neutron Spectra Calculated for the TSF Experimental Assembly and the GCFR Design. Spectra were calculated at the top of the grid plate shield (left) and at the top of the grid plate (right). The TSF calculation included 91 pins per assembly and the prototypic grid plate, and the GCFR calculation was for the low-pressure-drop, beginning-of-life design.

shield sections. This was anticipated because the grid plate shield assemblies were constructed to be prototypic of the GCFR design. Although only 77% of theoretical density (theoretical density = 2.52 kg/liter) was achieved for the B₄C in the experiment, the volume of B₄C was 26% greater than that for the GCFR design, thus making the total amount of B₄C per assembly in the experiment nearly equal to that found in the GCFR (expected to be 98 to 99% of theoretical density).

2.3 SHIELDING ANALYSIS IN SUPPORT OF GCFR DESIGN - C. O. Slater, M. L. Williams, and M. B. Emmett

2.3.1 Analysis of Core Grid Plate Damage

A number of significant results were obtained in the analysis of the GCFR grid plate region. Some accomplishments of last year follow.

- Transport analysis - (1) revised estimates of grid plate damage and flux spectrum were obtained from a detailed discrete-ordinates calculation, and (2) comparison was made of computed results from various methods and model approximations.
- Sensitivity and channel theory analysis - (1) calculation was performed for sensitivity of grid plate damage to changes in the density of B₄C in the grid plate shield, (2) calculation was performed for sensitivity of grid plate damage to changes in the width of interassembly gap, and (3) investigation was completed of major streaming paths contributing to grid plate damage.
- Source calculation - (1) the revised neutron source for upper and lower axial calculations was determined.

2.3.1.1 Transport Analysis

In 1977 ORNL attempted to determine the neutron transport to the grid plate, using the most detailed discrete-ordinates calculation to date. The purpose of this reference calculation was to (1) provide a conservative estimate of beginning-of-life grid plate damage and (2) provide a base case for comparison of other methods of approximation.

The calculation used a heterogeneous cell model (previously shown to be conservative) for a low-pressure-drop core assembly, as shown in

Fig. 2.17. The two-dimensional DOT discrete-ordinates code that performed the calculation used 51 energy groups and a very fine angular quadrature set (262 directions) to "see" the narrow streaming paths in the core and the interassembly gap. A P_3 Legendre expansion of the scatter cross sections was assumed.

The results of this calculation are given in Table 2.1, and Fig. 2.18 shows the calculated flux spectrum at the grid plate. Total flux was found to be on the order of 10^{13} neutrons $\text{cm}^{-2} \text{s}^{-1}$. Damage rates ranged from $2.6 \times 10^{-10} \text{s}^{-1}$ for nominal 5% elongation to $2.2 \times 10^{-9} \text{s}^{-1}$ for 10% upper-bound elongation.

Fig. 2.17. Grid Plate Geometry Used in DOT Run.

Table 2.1. Results of Reference Calculation

		Per unit source
Maximum flux in grid plate	$2.38 + 13 \frac{n}{cm^2 s}$	3.35-7
Maximum flux in grid plate >1 MeV	$5.46 + 11 \frac{n}{cm^2 s}$	7.69-9
Maximum flux in grid plate >100 keV	$8.94 + 12 \frac{n}{cm^2 s}$	1.26-7
Maximum flux in grid plate <2.38 eV	$3.22 + 8 \frac{n}{cm^2 s}$	4.54-12
Maximum 5% elongation rate	$2.59 - 10 s^{-1}$	3.65-30
Maximum 5% elongation rate, upper bound	$6.62 - 10 s^{-1}$	9.32-30
Maximum 10% elongation rate	$9.00 - 10 s^{-1}$	1.27-29
Maximum 10% elongation, upper bound	$2.16 - 9 s^{-1}$	3.04-29

Fig. 2.18. Neutron Flux Spectrum at Bottom of Grid Plate.

Because of the difficulty in accurately modeling the complex neutron transport in the GCFR core and grid plate regions, it is important to understand the effects of various assumptions made in the calculations. Last year a systematic study was undertaken which attempted to quantify the sensitivity of computed results to some of the methods and model approximations that were used in the analysis. Specific comparisons were as follows:

- Models - (1) heterogeneous/homogeneous model comparison, (2) moderate-pressure-drop/low-pressure-drop model comparison, (3) full-geometry/cell-geometry model comparison, and (4) beginning-of-life configuration/equilibrium configuration model comparison.
- Methods - (5) step mode/weighted-difference flux approximation comparison and (6) quadrature set comparisons.

Conclusions are discussed below.

1. Because pin streaming was believed to be significant in a GCFR, it was earlier decided to use a heterogeneous model to represent the reactor core. This model consisted of alternating annuli of fuel and helium, in which the correct volume fraction of fuel was conserved. Streaming then occurs through the helium bands. The alternate simpler homogeneous model merely smears all material into an equivalent core region. An approximate value for the effect of core streaming can be obtained by taking the ratio of results of the two cases. For most responses at the grid plate, the heterogeneous model gave about 8% higher values than did the homogeneous model.

2. Another parameter considered was the coolant pressure drop through the core. A higher-pressure-drop design means that the fuel pins are grouped more closely together, which tends to reduce the effect of pin streaming. However, a comparison of calculations for the GA low ΔP design and the moderate ΔP design showed that this effect is small. This result should be expected, since (as previously discussed) the entire pin streaming effect is only 8%.

3. For much of the grid plate analysis, cell models were used; that is, the transport calculation represented a single fuel assembly, which was assumed to be one of an infinite array of such unit cells. This approximation permits a detailed description of a small spatial

region (e.g., a single assembly) and is useful for modeling local phenomena such as interassembly streaming. For more global parameters like total leakage, a full geometry (which essentially consists of a finite number of homogenized cells) must be employed. If the two approximations are good, then meaningful comparisons of common results can be made. For most parameters of interest, the ratio of the cell-computed value to the full-computed value was about 10%.

4. An important effect that ORNL studied was the buildup of plutonium in the axial blanket. Although only a small fraction of the total power is produced in the axial blanket, the neutrons born there are very important to the grid plate damage (i.e., the axial blanket has a high value for the adjoint flux). Based on transport calculations, grid plate damage in the beginning-equilibrium cycle model was three times that of the beginning-of-life model. The end-of-equilibrium cycle results are slightly higher still.

5. The type of numerical approximation relating interval-centered fluxes to interval-boundary fluxes was found to be important in the transport calculations. Some earlier calculations inadvertently used a step approximation, and it was found that this calculational mode severely underestimates attenuation through the B₄C shield region. Calculations made with the new super-weighted difference scheme are more accurate approximations, and these show a twofold decrease over the step calculations.

6. The S_{10} symmetric quadrature set (70 angles) used in earlier DOT calculations appears to be adequate for most transport calculations in the grid plate region. The reference 262-direction-biased set showed good agreement with the S_{10} set for responses at the grid plate. The only exception was that the 262-direction set was able to pick streaming through the narrow interassembly gap streaming, which was not observed with the 70-direction quadrature.

2.3.1.2 Sensitivity and Channel Theory Analysis

One of the important questions addressed by ORNL during 1977 was "how sensitive is the grid plate damage to changes in the B₄C density of the grid plate shield?" This question was of immediate interest because

the mock-up of the grid plate shield in the TSF experiment would not have the same B_4C density as would the design configuration. The effect of this discrepancy on experimental results was required to assess the true grid plate shield design effectiveness. Knowledge of the damage sensitivity coefficient (defined as percent change in grid plate damage/percent change in B_4C density) could also be of future importance to GA when it becomes necessary to optimize the shield design.

First-order perturbation theory gave a value for the sensitivity coefficient to about -1 (i.e., a 1% decrease in B_4C density causes a 1% increase in grid plate damage). Direct calculations showed that the linear perturbation results were valid for density perturbations up to 15%.

The more general question next arose as to what are the attenuating properties of various mixtures of B_4C and stainless steel. Table 2.2 shows the results of this study, performed with one-dimensional discrete ordinates for a 10-cm slab with an incident fission source. The table shows that, for 75%-theoretical-density B_4C , the attenuation increases

Table 2.2. Comparison of the Attenuating Properties of a 10-cm Slab Composed of Mixture Containing Varying Fractions of SS_{316} and B_4C

Volume percent		Leakage	
SS_{316}	B_4C	Total	$E > 0.1$ MeV
B_4C at 75% theoretical density			
100	0	0.1622(1.31)	0.1553(1.33)
75	25	0.1483(1.20)	0.1408(1.20)
50	50	0.1369(1.11)	0.1292(1.10)
25	75	0.1285(1.04)	0.1212(1.04)
10	90	0.1250(1.01)	0.1183(1.01)
5	95	0.1241(1.01)	0.1176(1.00)
0	100	0.1234(1.00)	0.1171(1.00)
B_4C at 100% theoretical density			
75	25	0.1357(1.60)	0.1276(1.62)
50	50	0.1136(1.34)	0.1057(1.34)
25	75	0.09682(1.14)	0.08981(1.14)
10	90	0.08904(1.05)	0.09278(1.05)
5	95	0.08679(1.02)	0.08080(1.02)
0	100	0.08471(1.00)	0.07898(1.00)

only 30% by going from all stainless steel to all B_4C . There is, however, a twofold difference in these two extreme cases if the B_4C is 100% theoretical density. The maximum attenuation possibly occurs if the 10-cm slab is solid B_4C at 100% theoretical density, the attenuation being nearly a factor of 12 at that point.

The most challenging transport analysis of the GCFR grid plate region concerned the effect and importance of neutron streaming through the narrow gap separating fuel assemblies, that is, the interassembly gap. It appeared intuitively obvious to both GA and ORNL that this low-density region presented a streaming channel that could create a hot spot in grid plate damage; however, because the gap is very long and narrow, discrete-ordinate calculations have an extremely difficult time "seeing" such a path. Only when the DOT calculations used a very fine, 262-direction biased quadrature set did a peak in grid plate damage appear above the interassembly gap, and even then the peak is not very pronounced, as shown in Table 2.3. It is interesting that, even though the 70-direction quadrature does not pick up the damage peak above the streaming gap, it does a good job of estimating the average damage, which suggests that there is another streaming path that can be seen with the coarser quadrature.

Table 2.3. Comparison of Peak to Average Damage at Bottom of Grid Plate (10% upper bound)

Case	D	D_{max}	Location D_{max}
262 quad/0.68-cm gap	6.18-29	6.23-29	Above gap
262 quad/1.27-cm gap	1.61-28	1.64-28	Above gap
70 quad/0.68-cm gap	6.23-29	6.25-29	Inner edge of grid plate

An attempt was next made to analyze in more detail the streaming paths leading to the grid plate by employing a technique known as channel theory. A plot of the resulting contribution density is shown in Fig. 2.19. The contribution density, $N_c(r)$, is defined as the number of neutrons per cubic centimeter at some point times the average contribution of these neutrons to the grid plate damage (Eq. 2.1). Therefore, Fig. 2.19 shows the spatial distribution of the damage contribution

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Fig. 2.19. Contribution Density in Grid Plate Region.

$$N_c(r) = \iint_{E, \mu} \frac{1}{v} \phi^* dE d\Omega \quad (2.1)$$

carried by neutrons as they stream from the core to the grid plate. The adjoint calculation for this analysis placed a damage detector directly above the interassembly gap and used the same 262-direction quadrature as the forward calculation.

The channel theory results suggest that damage to the grid plate arises from a combination of two streaming components. Particles that stream axially from the core contribute by flowing through the helium coolant channel (represented by the broad hump at the left-hand side of the figure) and then through the steel section separating the two B_4C shield regions. Particles that leak radially from the core enter the assembly gap and contribute through that channel (represented by the narrow ridge on the right-hand side of the figure).

Having verified the presence of a streaming path through the interassembly gap, it was necessary to ascertain the effect of grid plate damage of varying the gap size (recall that channel theory tells nothing about response *change*). Calculations summarized in Table 2.3 showed that increasing the gap size by a factor of 1.9 (from 6.8 mm to 12.7 mm) increases damage by a factor of 2.6; thus the damage is quite sensitive to gap size. The results of calculations done with a 70-direction quadrature show a similar trend, but slightly less drastic.

2.3.1.3 Source Calculations

Results of transport analysis done on the grid plate region of the GCFR were used to modify the existing source entering the lower axial regions. The original source for these calculations was taken from a full homogeneous geometry mock-up of the beginning-of-life configuration. This new source was modified in the following manner: (1) a spectrum was taken from a heterogeneous-cell calculation for the *equilibrium* cycle core; (2) the spectrum was attenuated through 12.9 in. of steel and helium (25%/75%) to account for transport through the assembly bottom fitting; (3) the old source at core center was normalized to

agree with the cell calculation; and (4) the sources at other intervals were modified, using the same "correction factor" as in (3).

As previously discussed, the streaming effect observed in the heterogeneous calculation was only 8% more than the homogeneous calculation; therefore, this difference will have a small effect on the source modification. However, the effect of using an equilibrium core as opposed to a beginning-of-life core changes the nature of the source entering the lower axial region. The modified source was found to be somewhat larger and to have a harder spectrum than the previous source.

2.3.2 Analysis of Upper Axial Shield Design

A preliminary analysis of the GCFR upper axial shield design was performed, based on the model shown in Fig. 2.20. The model represents a vertical slice in a plane that contains the main helium circulator ducts. In calculations, two variations of this model were considered: (1) a model with the inlet (upper) duct open and the outlet (lower) duct closed, as shown in Fig. 2.21, and (2) a model with both ducts closed, as shown in Fig. 2.22. The outlet duct was closed in both models because

Fig. 2.20. Basic Model of the GCFR Upper Axial Regions. A vertical slice in the plane of the reactor cavity inlet and outlet ducts from and to the main helium circulator.

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Fig. 2.22. Model of the GCFR Upper Axial Regions with Closed Outlet Duct and Closed Inlet Duct. Conical surfaces are represented by stepped cylinders (conserving volume) in the two-dimensional model.

it seemed sufficiently decoupled from events in the regions above it by the presence of the outer radial shield. Its omission from both models also allowed additional simplifications to the models. The closed inlet duct permitted conservative estimates of radiations within the cavity and regions on the cavity wall away from the duct openings, and the open duct case permitted conservative estimates of radiation levels deep within the ducts and within the prestressed concrete reactor vessel (PCRV) surrounding them.

Calculations were begun from the core, coupled at the grid plate, and continued throughout the upper axial regions. Two core calculations were performed. The first was a full-core heterogeneous calculation based on a beginning-of-life core source. It was characterized by 76 groups (51 neutron and 25 gamma), S_6 quadrature, and P_3 cross-section expansion. The second calculation was a heterogeneous cell calculation based on an "end-of-equilibrium-cycle" core source. It was characterized by 76 groups, S_{10} quadrature, and a P_3 cross-section expansion. The cell model represented a single subassembly and its portion of the interassembly gap, with all hexagonal boundaries converted to equivalent cylindrical boundaries. The cell had reflected boundary conditions on the left, right, and bottom boundaries to signify no net leakage radially or across the core axial centerline. The grid plate was along the top boundary of both the cell and the full-core model, and in both models a vacuum boundary condition was imposed at the top boundary.

The angular fluxes at the top of the grid plate from both calculations were used to create an internal boundary source for the remaining calculation. The full-core model supplied the radial distribution of the source at the level of the top of the grid plate, and the cell calculation provided the normalization. The normalization was such that the modified angular flux at the center of the grid plate in the full-core model would give the same group scalar flux as the cell model. The necessity for creating the source in this manner was due to the lack of an end-of-equilibrium-cycle full-core model. The calculations were continued with the internal boundary source at the top of the grid plate. The homogenized grid plate was included in the model to account for particles and their secondaries, which leave the upper regions, enter the grid plate, and return to regions above the grid plate.

The calculations showed that the design meets all present design criteria. The criterion for damage to the PCRV liner [47°C nil ductility temperature shift (NDTS)] comes closest to being exceeded (the limiting fluence is about twice as high as the maximum fluence level in the liner at the inlet duct level but away from the duct opening). Figures 2.23 and 2.24 show isoplots of the total flux and the fluence required to reach the 47°C NDTS, respectively, for the closed-inlet duct case. The corresponding isoplots for the open-inlet duct case are shown in Figs. 2.25 and 2.26. Table 2.4 lists plotting symbols and values for Figs. 2.23 and 2.25, and Table 2.5 lists plotting symbols and values for Figs. 2.24 and 2.26. The total flux at a given point on the liner should be multiplied by the desired reactor lifetime in seconds [30 years or 5.467×10^8 s for the 300-MW(e) demonstration plant] and a capacity factor (assumed to be 0.8); the result should then be compared with the limiting fluence at the same point to see if the criterion has been exceeded. Shifting of neutron spectra causes a shift in the limiting neutron fluence as evidenced by the curves in Figs. 2.24 and 2.26. Drastic changes do not occur in the helium because there is little

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Fig. 2.23. Isoplot of the Total Flux (neutrons $\text{cm}^{-2} \text{s}^{-1}$) in the Model of the GCFR Upper Axial Regions with Both Ducts Closed.

Fig. 2.24. Isoplot of the Neutron Fluence (neutrons cm^{-2}) Required to Effect a 47°C Nil Ductility Temperature Shift in the PCRV Liner in the Model of the GCFR Upper Axial Regions with Both Ducts Closed.

Fig. 2.25. Isoplot of the Total Flux (neutrons $\text{cm}^{-2} \text{s}^{-1}$) in the Model of the GCFR Upper Axial Regions with the Inlet Duct Open and the Outlet Duct Closed.

Fig. 2.26. Isoplot of the Neutron Fluence (neutrons cm^{-2}) Required to Effect a 47°C Nil Ductility Temperature Shift in the PCRV Liner in the Model of the GCFR Upper Axial Regions with the Inlet Duct Open and the Outlet Duct Closed.

Table 2.4. Plotting Symbols and Their Values as They Appear in Figs. 2.23 and 2.25

Value	Symbol	Value	Symbol
$5.0 + 2$	ARRO	$1.0 + 9$	Y
$1.0 + 3$	XBAR	$2.0 + 9$	STAR
$5.0 + 3$	Z	$5.0 + 9$	O
$1.0 + 4$	Y	$1.0 + 10$	3 ANG
$5.0 + 4$	STAR	$2.0 + 10$	+
$1.0 + 5$	O	$5.0 + 10$	X
$5.0 + 5$	3 ANG	$1.0 + 11$	DIMN
$1.0 + 6$	+	$2.0 + 11$	ARRO
$5.0 + 6$	X	$5.0 + 11$	XBAR
$1.0 + 7$	DIMN	$1.0 + 12$	Z
$5.0 + 7$	ARRO	$2.0 + 12$	Y
$1.0 + 8$	XBAR	$4.5 + 12$	STAR
$5.0 + 8$	Z		

Table 2.5. Plotting Symbols and Their Values
as They Appear in Figs. 2.24 and 2.26

Value	Symbol
4.0 + 19	Z
6.0 + 19	Y
8.0 + 19	STAR
1.0 + 20	O
2.0 + 20	3 ANG

spectrum change during transport through the helium. Steeper gradients occur near the interface of the graphite shields and the helium cavity. However, the variation in the limiting fluence over the entire system appears to be, at most, a factor of 10.

Additional calculations, including sensitivity if needed, are planned when the full-core end-of-equilibrium-cycle model is received.

2.3.3 Analysis of Lower Axial Shield Design

During late 1977, ORNL began reexamining the lower axial region of the GCFR, using an updated source from the core. Calculations proceeded in a fashion similar to those in the original lower axial analysis, described in detail in ORNL-5294. For a detailed description of the new source for the transport calculation, see Sect. 2.3.1.

Most of the calculated values of interest increased with the revised source because of the harder spectrum. A summary of results is given in Table 2.6. Recall that the present source was obtained from a core model that contained plutonium in the *axial* blankets only — the effect of plutonium buildup in the radial blankets has not yet been examined. The channels of transport followed by neutrons leaking radially were not important in analysis thus far; when the next model (containing plutonium in radial blankets) is examined, however, these streaming paths will be studied closely.

Table 2.6. Calculated Responses in GCFR Lower Axial Regions
(Values normalized to a neutron source of 7.1×10^{15})

Location along PCRV liner	Maximum gamma heating (mW/cm ²)	Maximum gamma dose rd	$\epsilon > 1 \text{ MeV}$	$\epsilon < 2.38 \text{ eV}$
Lower helium channel	0.90	5.2×10^1	2.5×10^2	$3.5 + 9$
Wrap-around region	2.6	8.7×10^1	5.3×10^2	$1.8 + 10$
Above wrap-around region	2.75	8.8×10^1	8.8×10^2	$3.5 + 10$

2.4 REACTOR PHYSICS ANALYSIS OF ALTERNATE GCFR FUEL CYCLE — T. J. Burns and J. R. White

In light of recently accentuated proliferation concerns regarding the use of the plutonium/uranium cycle in nuclear power reactors, a preliminary evaluation of the impact of several proposed alternative fuel cycles was begun late in 1977. The principal alternate fuel cycle considered was the denatured cycle ($^{233}\text{U}/^{235}\text{U}/^{232}\text{Th}$). The fuel is "denatured" by the presence of ^{235}U , thereby reducing the potential for use of the fresh fuel as a source of material for weapons use.

The scoping calculations were initiated by adopting one of General Atomic's 1200 MW(e) designs as a base case. The reference fuel, $(\text{Pu}/^{235}\text{U})\text{O}_2$, was replaced with various alternate fissile/fertile combinations. Although the oxide density was modified to account for differences in alternative materials (e.g., ThO_2 vs UO_2), the various systems were not optimized to account for the better thermophysical properties of some of the alternative materials.

The results of the beginning-of-life (BOL) (static) calculations are summarized in Table 2.7. As indicated in the table, the replacement of plutonium by ^{233}U as the fissile material has a significant impact on the breeding ratio. The rather drastic change observed is attributable to the markedly different η (average number of fission neutrons produced per absorption in fissionable isotopes) of the two isotopes in the hard GCFR core spectrum. Further, the replacement of ^{235}U by ^{232}Th as the core fertile material has a significant effect on the breeding performance. Because of the hardness of the GCFR spectrum, fast fissions in

Table 2.7. Beginning-of-Life Breeding Ratio for Some Alternate Fuel Cycle GCFRs

Core	Axial blanket	Radial blanket	Breeding ratio (BOL)
Pu/ $^{238}\text{UO}_2$	$^{238}\text{UO}_2$	$^{238}\text{UO}_2$	1.42
Pu/ $^{238}\text{UO}_2$	$^{238}\text{UO}_2$	ThO_2	1.39
Pu/Th	ThO_2	ThO_2	1.28
$^{233}\text{U}/^{238}\text{UO}_2$	$^{238}\text{UO}_2$	ThO_2	1.14
$^{233}\text{U}/^{238}\text{UO}_2$	ThO_2	ThO_2	1.12
(20%) $^{232}\text{U}/^{238}\text{UO}_2 + \text{ThO}_2$	ThO_2	ThO_2	1.09
(40%) $^{233}\text{U}/^{238}\text{UO}_2 + \text{ThO}_2$	ThO_2	ThO_2	1.05
$^{233}\text{U}/\text{ThO}_2$	ThO	ThO	1.01

^{238}U (in the reference calculation) contribute significantly to the breeding performance. Use of ^{232}Th as the core fertile material reduces this effect markedly due to the factor-of-4 difference in the fissior cross sections of the two fertile isotopes.

Given in Table 2.8 are various results of burnup calculations for alternative GCFR fuel cycles. The equilibrium cycle results reflect some differences from the BOL calculations, which can be attributed to the presence of fission products and the production of fissile isotopes not present in the fresh fuel (e.g., ^{239}Pu in the denatured cases).

Implementation of a denatured fuel cycle typically requires two types of reactors, dispersed denatured systems and a safeguard ^{233}U production reactor. The latter type is required because the ^{233}U component of the denatured reactors is generally less than unity, implying a source of ^{233}U for makeup requirements. A symbiotic system, such as depicted in Fig. 2.27, is envisioned in which the energy center "transmuters" (so-called because of the input of plutonium and output of ^{233}U) provide fuel for the denatured systems and vice versa. Due to the better neutronic properties of ^{239}Pu (relative to ^{233}U) in the GCFR, the GCFR is ideally suited to the code of transmuter in such a proliferation-resistant scenario.

Table 2.8. Performance Parameters of Alternate Fuel Cycle GCFRs

Core	Reactor		Initial fissile inventory [kg/GW(e)]	Equilibrium fissile loading [kg/GW(e)-year]	Equilibrium discharge, ^a ²³³ U fissile Pu [kg/GW(e)-year]		Breeding ratio (mid-equilibrium)	Fissile ^b doubling time (year)
	Axial blanket	Radial blanket						
Pu/U	U	U	2641	965	--	1163	1.301	14.3
Pu/U	U	Th	2693	987	224	941	1.276	15.4
Pu/Th	Th	Th	3170	1158	626	619	1.150	48.3
²³³ U/U ^c	U	Th	2538	1001	671	400	1.088	50.5
²³³ U/U ^d	Th	Th	2587	1019	822	256	1.074	66.8
²³³ U/U + Th ^e	Th	Th	2720	1031	871	208	1.060	98.4
²³³ U/U + Th ^f	Th	Th	2956	1131	1054	81	1.004	-151.5
²³³ U/Th	Th	Th	3108	1192	1169	--	0.970	-64.3

^a@ 75% capacity.^b2% losses, 1 year out of reactor time.^c17.9% ²³³U/U.^d17.7% ²³³U/U.^e20% ²³³U/U.^f40% ²³³U/U.

Fig. 2.27. Conceptual Schematic of Denatured Fuel Cycle Symbiotic System.

To assess potential improvements in the performance of alternative fuel cycle GCFRs, a survey calculation of the possible use of a heterogeneous GCFR was performed. An annular core model consisting of fissile driver assemblies and radial blanket assemblies was formulated. An effort was made to show that the R-Z model proposed to analyze this heterogeneous configuration was both an accurate and realistic representation of the three-dimensional fast reactor. Preliminary calculations indicate that the simplified R-Z calculational model produces accurate results with a significant reduction in cost and effort. Table 2.9 contrasts the results of a two-dimensional and a three-dimensional (triangular-Z) static calculation for one of the "transmuter" options under study, $(\text{Pu}/\text{U})\text{O}_2$ drivers and ThO_2 internal blanket assemblies. In addition to the agreement shown in Table 2.9, the radial power distributions were also in good agreement. Furthermore, as an indication of the potential of the heterogeneous GCFR concept, the breeding ratio of this case (1.52) should be compared to that of the equivalent homogeneous (zoned core) calculation given in Table 2.7 of (1.39).

Table 2.9. Comparison of Two-dimensional and Three-dimensional Computational Reactor Models

	Two-dimensional model	Three-dimensional model
<u>Reactor description</u>		
Driver	Pu/UO ₂	Pu/UO ₂
Axial blanket	UO ₂	UO ₂
Internal blanket	ThO ₂	ThO ₂
Radial blanket, 3 rows	ThO ₂	ThO ₂
<u>Beginning-of-life parameters</u>		
K _{eff}	1.010	1.010
Maximum power density, W/cm ³	406.7	421.6
Maximum linear heat rate, kW/ft	11.5	11.9
Fissile enrichment	0.284	0.286
Total breeding ratio	1.52	1.52
U ²³³ breeding ratio	1.12	1.12
U ²³³ production/total production	0.736	0.737

2.5 REFERENCES

1. W. A. Rhoades et al., *The DOT-IV Two-Dimensional Discrete-Ordinates Transport Code with Space-Dependent Mesh and Quadrature*, ORNL-5283, to be published.
2. W. W. Engle, Jr., *A USERS MANUAL FOR ANISN: A One-Dimensional Discrete Ordinates Transport Code with Anisotropic Scattering*, Union Carbide Corporation, Nuclear Division, K-1693, 1967.
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3. GCFR PRESSURE VESSEL AND CLOSURE STUDIES

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3.1 INTRODUCTION

The primary objective of this program is to provide technical support for the design and development of the GCFR prestressed concrete reactor vessel (PCRv). Analytical and experimental studies of the closures for both the steam generator and the central reactor cavities are being conducted. Figure 3.1 is a cross section of the PCRv showing the two types of closures.

Fig. 3.1. PCRv for the Demonstration 300-Mw(e) GCFR Power Plant; Cross-Sectional View of Preliminary Design by General Atomic Co. Reprinted by courtesy of General Atomic Co.

The program consists of three phases: (1) relatively small 1:15-scale closure plug models, (2) more detailed 1:4-scale closure models in which the plug, seal, and hold-down system are modeled, and (3) a 1:10-scale model of the entire PCRv.

These models are based on designs provided by the General Atomic Company (GA), and activities have been scheduled so as to integrate them into the overall GCFR development schedule. Development of designs for the PCRV and closures must be closely associated with and responsive to the development of conceptual designs for the overall GCFR. In designing the GCFR system, consideration must be given to technological, economic, social, and safety-related influences. As a consequence, the criteria for various features of the PCRV have undergone significant change, and the closure designs have been modified during the reporting period. The effects of these design changes are discussed in those cases in which they have significantly affected the overall progress.

During the previous reporting period, activities consisted primarily of the design and fabrication of both the test fixture for the 1:15-scale model tests and the 1:15-scale steam generator cavity closure plug.

Activities during the current reporting period have consisted of (1) design of the 1:15-scale central reactor cavity closure plug model and simulation of mechanical properties of components for this model; (2) testing of the 1:15-scale steam generator cavity closure plug model; (3) fabrication, instrumentation, and testing of a half-thickness version of the 1:15-scale steam generator cavity closure plug model; and (4) design and procurement of equipment and facilities to accommodate 1:4-scale closure models and the 1:10-scale PCRV model.

3.2 CENTRAL CORE CAVITY CLOSURE MODELS -- 1:15 SCALE

Figure 3.2 shows plan and vertical cross sections of the preliminary design for the 1:15-scale central cavity closure plug model. The large number of tubes (consisting of 237 fuel element and 20 control rod tubes), the presence of interlacing steel gussets, and the complete enclosure by top and bottom liners presented considerable complexity and difficulty in fabrication and concrete placement and in the simulation of model mechanical properties. Design of the model was performed under subcontract by Midwest Technical, Inc. We completed procurement of the material stock, validated most of the requisite heat treating procedures, developed the instrumentation layout, and designed special fixtures for adaptation of the model to the test fixture. Prior to start of fabrication, GA

Fig. 3.2. Plan and Vertical Sections of 1:15-Scale Model of Central Cavity Closure Plug of PCRV for GCFR Demonstration Plant.

decided to redesign the fuel rod locking mechanism, which drastically changed the design of the central core cavity closure. Further activity has therefore been suspended until GA provides the revised design.

3.3 STEAM GENERATOR CAVITY CLOSURE MODELS - 1:15 SCALE

During the current reporting period we have tested two 1:15-scale steam generator cavity closure models. The first model, which was tested on Jan. 6, 1977, is based on the original GA design shown in Fig. 3.3. It is designated as the full-thickness model to differentiate it from the second model, called the half-thickness model. A cross section of this model positioned for testing in the test fixture is shown in Fig. 3.4.

The model was subjected to ten pressurization cycles of the PCRV design 10.08-MPa (1462-psig) maximum-cavity pressure (MCP). On the final cycle, it was intended that the model be pressurized in steps to failure. The model was pressurized in steps to a pressure of 75.8 MPa (11,000 psig), at which time the test was discontinued to avoid damaging the hold-down system. At the maximum test pressure, which was about 7.5 times the

Fig. 3.3. GCFR Steam Generator Cavity Closure Plug; Original Design and Full-Depth Model Test Design. To convert dimensions to meters, multiply numbers of feet by 0.305 and inches by 0.0254.

Fig. 3.4. Cross Section of 1:15-Scale Steam Generator Cavity Closure Plug Model Installed in Test Fixture.

design pressure of the prototype, significant nonlinearities of strain and deformation were observed in both the concrete and steel components of the structure. Figure 3.5 is a view of the 0-to-180° cross section of the model after sectioning. Neither visual cracking nor gross distortion is evident.

A prognostication of failure was made by means of a nonlinear analysis of the structure, using the finite element code ADINA.¹ The calculated results compared with the experimental results were consistently conservative; however, improved constitutive equations for concrete behavior are required to provide an accurate understanding of the mode of failure. Since both the calculated and experimental results indicated that the plug was substantially overdesigned, it was recommended that a

Fig. 3.5. The 0-to-180° Cross Section of the 1:15-Scale Steam Generator Cavity Closure Model After Testing.

reduced-thickness second model be tested. The cost of a second test was reduced significantly by duplicate parts for the first model which were fabricated originally to ensure availability of parts meeting the tight dimensional tolerances required for the relatively small model. All parts met the specified tolerances and were consequently available for the second test. The recommendation for the proposed follow-up test was

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adopted in April; the design of the model was based on the following recommendations:

1. thickness to be reduced one-half that of the first model,
2. shear console steel reinforcement to be eliminated, and
3. rebar layout to remain essentially the same except for eliminating the middle section designed to allow for the presence of the shear console.

Fabrication of the metallic components of the model was completed in September, and the microconcrete was cast in October, using the same mixture as for the first model. The completed model assembly prior to concrete casting is shown in Fig. 3.6.

Fig. 3.6. Completed and Instrumented Half-Thickness Model Assembly Prior to Concrete Casting.

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A series of nonlinear analyses was made of the half-thickness model, using the ADINA finite element code. These analyses predicted failure to occur at 72.4 to 75.2 MPa (10,500 to 11,000 psi).

The redundancy in location of the bonded strain gages on the steel components used for the first model was eliminated. Based on the recommendations of GA, these gages were repositioned at intermediate locations on the outer and inner liner vertical sections, on the exposed concrete surface, and on the bottom liner surface. It was also necessary to change the design and location of the concrete embedment gages. Linear variable-differential transformers replaced the clip gages for monitoring radial deflections at the upper surface of the model. Layout of the remainder of the instrumentation was the same for both models. Figure 3.7 shows a cross section of the half-thickness model positioned in the fixture for testing.

Fig. 3.7. Cross Section of 1:15-Scale Half-Thickness Steam Generating Cavity Closure Plug Model Installed in Test Fixture.

The test plan for the second model was identical to that for the first model. After being subjected to ten pressurization cycles of from about 1.0 MPa to the MCF, the model was pressurized in steps to failure, which occurred at about 99 MPa (14,400 psig). The failure occurred by punching shear along the roughly conical failure surface shown in Figs. 3.8, 3.9, and 3.10.

Fig. 3.8. Posttest View of Major Parts of 1:15-Scale Half-Thickness Steam Generator Cavity Plug Model.

Fig. 3.9. Posttest View of Inner Portion of 1:15-Scale Half-Thickness Steam Generator Cavity Plug Model.

Fig. 3.10. Posttest View of Inverted Outer Portion of 1:15-Scale Half-Thickness Steam Generator Cavity Plug Model.

3.4 MODEL STUDIES -- 1:4 SCALE

Activities during the current reporting period have consisted of the design of the test fixture to accommodate the 1:4-scale models, based on the following criteria:

1. Minimum pressurization capability of 2 MCP.
2. Restraint of plugs in both primary and secondary hold-down modes.
3. Instrumentation for measurement of model response to identify elastic response and departure from linear response of the plug and hold-down system and for measurement of deformations of the seal (prototype seal location). This information will be used in the design of subsequent half-scale seal fatigue tests.
4. Compliance of the prototype hold-down anchorage not to be simulated; however, the toggles of the primary hold-down system to be modeled.

After conducting a series of conceptual designs of the secondary hold-down restraint system, we were unable to develop an acceptable system for testing the closure in the secondary hold-down mode. Based

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on these findings, the secondary hold-down static test was deleted by GA in July; however, further consideration has been given to the feasibility of performing dynamic tests. In August, we completed the design of the test fixture and the 1:4-scale steam generator cavity model, based on primary hold-down restraint only. As a result of an extensive review of the proposed closure design at GA, we have suspended all work on the 1:4-scale model tests and have suspended procurement of the steel forging that was to serve as the vessel for these tests. We will resume procurement as soon as a specific closure configuration has been adopted by GA and test criteria can be established.

Work is continuing on the development of the basic facilities for conducting tests on the 1:4-scale closures and the PCRV model. Design of the pressurization system is proceeding, based on information provided by the vendor for the intensifier. Plans have been implemented to install the missile shielding, intensifier, and pressurization system.

3.5 REFERENCE

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