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**Gas-Cooled Fast Reactor Program
Annual Progress Report for Period
Ending December 31, 1979**

Uri Gat
Paul R. Kasten

MASTER

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GAS-COOLED FAST REACTOR PROGRAM ANNUAL PROGRESS REPORT
FOR PERIOD ENDING DECEMBER 31, 1979

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operated by
UNION CARBIDE CORPORATION
for the
DEPARTMENT OF ENERGY

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HIGHLIGHTS

The ORNL GCFR Program progress in 1979 can best be characterized as a year of accomplishments. Some major events and milestones in 1979 follow:

1. Core Flow Test Loop

The need and high priority for the CFTL were recognized by all the GCFR Program participants.

A staged schedule for the CFTL was established and maintained.

The GCFR design changes were incorporated into the CFTL with no major disruption of the progress.

The design of the loop progressed well.

Building modifications were mostly completed.

Procurement and manufacturing progressed as scheduled.

The analysis group was established and provided valuable results for the entire GCFR Program.

Bundle AGI design approached completion.

2. Shielding and Physics

The grid plate experiment was completed.

The radial shield experiment was completed.

Shielding calculations for the preliminary upflow core design were completed.

The critical experiments done at the Swiss Federal Institute for Reactor Research were analyzed.

3. Pressure Vessel and Closure Studies

The conceptual design of the generic central cavity closure model was completed.

The construction of the quarter-scale closure model test cell was completed, including the pressurizing equipment.

We participated in the planning of the GCFR PCRV Program.

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4. Irradiation Program

The microfilming and storage of the GB-9 and GB-10 experiment documents was completed.

TECHNICAL SUMMARY

1. Core Flow Test Loop

The Core Flow Test Loop (CFTL), the high-temperature, high-pressure, fast transient, out-of-reactor helium circulation loop, is designed to supply helium at appropriate GCFR conditions to individual test bundles. A test bundle consists of electrically heated fuel or blanket rod simulators that are arranged to represent a segment of a fuel or control rod assembly or a full-size blanket assembly of the GCFR core. Near the end of CY 1978, a staged construction approach to achieve the 91-rod bundle capacity and full transient capability of the loop was adopted. Initially, a Stage 1 CFTL will be constructed to have a 37-rod bundle capacity and limited transient capability. The Stage 1 CFTL is scheduled for completion near the end of FY 1981.

During the year the details of the GCFR core and core assemblies were changed. The diameter and length of the fuel rod and the length of the core assemblies were changed. The flow of helium coolant through the core was changed from downflow to upflow. Radial restraints were added to the core at two different elevations. These changes impacted on the CFTL analysis, loop construction, and bundle efforts.

Analysis tasks that had been completed previously for the first CFTL bundle had to be revised as a result of changes in the GCFR core. The principal revised tasks were the calculation of the inlet flow distribution and pressure drop of the test bundle and the specification of the test section instrumentation. Several planning meetings with General Atomic Company (GA) and short working visits held during the year were very beneficial to the CFTL analysis effort. As a result of these cooperative efforts, it was agreed that the computational procedures at ORNL should stay consistent with those at GA. However, major problems were associated with the implementation of COBRA*GCFR at ORNL. Most of these problems are related to both hardware and software differences in the CDC, UNIVAC, and IBM computers involved, respectively, in the development of the original coding for COBRA at Battelle Northwest Laboratories,

modifications of the coding for the GCFR at GA, and implementation of the coding at ORNL. An extensive review and evaluation was made of the analytical procedures for correlating the performance characteristics of artificially roughened surfaces. This led to a novel method of correlating available performance data for roughened surfaces. An evaluation of the characteristics of the exact roughening for the CFTL fuel rod simulator in an existing gas loop in Germany was planned. The overall analysis task delegated to ORNL was significantly increased to include calculation of cladding surface temperatures based on internal thermocouple readings. Error bands and confidence limits associated with this calculation are to be determined and reported. During the year, it became apparent that efforts to construct appropriate structural analysis models for the finite element code ADINA would be inadequate to represent all the interactions in the composite bundle. By the end of the year, a computational procedure using a transfer-matrix method to evaluate these interactions had been developed and its application demonstrated for bundle AG1. Locations of thermocouples and pressure taps were specified for bundle AG1.

The Title I design work for the construction of the CFTL loop was completed, and the Title II design for the mechanical and electrical engineering was nearly completed. The Title II design for instrumentation and controls was initiated and accelerated with the assistance of subcontractor design personnel.

The design of the loop was impacted by the changes made to the GCFR core and core assemblies, and we substantially completed the associated changes to the piping system and the test vessel to accommodate the change in flow direction of helium coolant and the increased lengths of the fuel rods and assembly ducts. The design and construction of building facilities were substantially completed. The designs of heat exchangers HX-1 and HX-2 were completed and the fabrication of HX-1 was substantially completed. The prototype of the helium circulator passed its performance test satisfactorily, and two additional circulators were ordered to provide the three needed in the CFTL. The major components for the test vessel were ordered and some were received. The helium supply system piping was installed.

The primary electrical power distribution system was installed at the CFTL. The first-zone power supply for the test bundle was ordered. Two variable-frequency power supplies were ordered for the helium circulators; the third power supply has already been delivered. The design of the auxiliary power system was completed.

A detailed description of the current capabilities and modeling techniques was prepared for ORCULT, the CFTL loop dynamics simulation code. Procurement for the helium flow control valves, including the hydraulic operators, was initiated. Following successful completion of tests and evaluation of a commercial vortex shedding flowmeter to measure CFTL helium flow rates accurately, procurement of the flowmeters began. The design philosophy for the central control system was reviewed and was deemed satisfactory by the CFTL Design Review Committee, and procurement of the associated direct digital control (DDC) then began. Interlock circuits were developed for the power supplies, helium circulators, and other loop components. Design of instrument cabinets for test bundle signal conditioning equipment was completed and construction began.

The design of test bundle AG1 neared three-quarters completion despite the impact of the changes that were made to the GCFR core and core assemblies. We completed the development of methods to make the cladding-to-tubesheet joints. A method to connect the terminals of the fuel rod simulators to the power supply leads was developed. We believe the reliable life of the thermocouples in the fuel rod simulators was enhanced by the specification of an appropriate draw-and-heat-treatment procedure that reduces residual cold work to about 1% and minimizes the grain size of the thermoelements. All the bundle development work is proceeding to satisfactory conclusions except for the fuel rod simulators. Here a fabrication problem with maintaining good compaction density of the insulation and good heat transfer contact among the rod components has led to random melting failure in the nickel end plug. The plug is located near the joint between the central nickel electrode and the Kanthal A-1 heating element. Means for alleviating this problem are being pursued vigorously. The installation of bundle AG1 into the CFTL is now scheduled for near mid-FY 1982.

2. Shielding and Physics

Activities performed in the GCFR Shielding and Physics Program during 1979 include completion of the grid-plate shield design verification experiment postanalysis; completion of the radial blanket and shield experiment and associated preanalysis, and initiation of postanalysis; design support analysis for preliminary upflow shield designs; preliminary feasibility assessment of the fast mixed-spectrum reactor concept; and analysis of critical experiments performed at the Swiss Federal Institute for Reactor Research (EIR), Würenlingen, Switzerland.

The grid-plate shield experiment investigated the effectiveness of the grid-plate shield design and attempted to establish the validity of methods used for related shielding calculations. The detailed analysis of the experimental results concluded that the methods and data used in the analysis of the GCFR design are adequate for calculating (1) axial neutron streaming in the GCFR core and axial blanket for a range of fuel-pin and subassembly spacings and (2) the peak fast-neutron exposure to the grid plate. However, the angular quadrature significantly affected the magnitude of the calculated source emerging from the grid plate, allowing for large overpredictions and gross underpredictions. The grid-plate shield design was found adequate.

The radial blanket and shield experiment was performed to validate nuclear data used in the GCFR shielding design calculations. Preanalysis of the experiment resolved several design and fabrication problems and showed that the intended measurements would be highly sensitive to the important nuclear data. All the high-priority measurements were completed. These included integral neutron flux data and flux spectral data for neutron and gamma rays. Also, gamma-ray heating measurements were made in several blanket and radial shield configurations. Postanalysis of the experiment was initiated with emphasis on the first configurations — the UO_2 and ThO_2 blanket mockups. Preliminary results indicate that ENDF/B-V ^{232}Th data offer an improvement over the previous evaluation, but may still be inadequate.

The design support analysis concentrated on the revised downflow shield designs and then investigated the preliminary upflow shield designs. The upflow study was divided into three separate and extensive tasks, which included the radial shield design and lower and upper plenum shields designs. Calculations showed that all design criteria are easily satisfied in the plena and adjacent regions; however, the total lifetime fluence at the core restraint cylinder did exceed the allowed 10^{25} neutrons/m² limit. We began analysis of neutron streaming in the "flow through" plenum shields that have been incorporated into the new conceptual shielding design.

A preliminary study was made to determine the feasibility of a fast mixed-spectrum reactor (FMSR) concept, which is envisioned as operating on natural uranium feed. The study could not ascertain the feasibility of the concept, although several potential variants were suggested. Also, several deficiencies in methods and data unique to the FMSR were identified.

Final analysis was performed for two of the Swiss critical experiments, which contained thorium in GCFR-type fuel pin lattices. We analyzed the axial blanket experiments, which included a ThO₂ blanket arrangement and a thorium-metal blanket. The results indicate a deficiency in the ²³²Th capture data when measured in the infinitely dilute environment at core center and a deficiency in the methods for analyzing the self-shielded data in the axial blanket.

3. Pressure Vessel and Closure Studies

The program provides technical support for the design and development of the GCFR prestressed concrete reactor vessel (PCRv). Current and completed studies have consisted of analytical and experimental evaluations of the closures for the steam generator cavity and the central core cavity.

Three principal activities of the program in response to ASME Code requirements are analyses and tests of (1) 1/20- to 1/15-scale closure plug models, (2) 1/4-scale closure plug models, and (3) a 1/10-scale model

of the complete PCRV. The first of these activities has as primary objectives the determination of the margin of safety and the structural response due to overload. In addition, although limited by the relatively small scale, experimental data in the elastic regime are obtained for validation of design analyses. The second of these activities is intended to provide detailed confirmation of design methods employed by the designer by using a different computer code from the designer and by obtaining precise experimental strains and displacements at critical locations. The third activity is intended to obtain the margin of safety and the structural response due to overload for the complete PCRV.

During the previous reporting period efforts began on the 1/15-scale central reactor cavity closure plug model and on the procurement of a 1/4-scale model test fixture to accommodate models of the steam generator cavity closure and the central core cavity closure. Although these activities had progressed through the design stage to the procurement of components and materials, these efforts were interrupted and stopped because of significant design changes to the prototype PCRV. As a consequence ORNL efforts were redirected to implement a test of a 1/20-scale central core cavity closure plug model. Construction work on the test cell and pressurizing equipment to test 1/4-scale closure models and 1/10-scale complete PCRV models was completed.

4. Irradiation Program

The microfilming of the records of irradiation capsules GB-9 and GB-10 was completed.

1. CORE FLOW TEST LOOP

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

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

The ORNL Core Flow Test Loop (CFTL) Program for the Gas-Cooled Fast Reactor (GCFR) includes (1) construction of the CFTL; (2) construction of test bundles that simulate GCFR core assemblies and their operation in the CFTL under the steady-state and the normal, upset, emergency, and faulted conditions anticipated for the GCFR; (3) development tasks supporting the designs of the CFTL and the test bundles; (4) analyses supporting the design, construction, and operation of the CFTL and the test bundles; and (5) acquisition and analysis of programmatic test data during test bundle operation. The objectives¹ of the CFTL Program are unchanged from the previous report.²

In 1978 the CFTL project adopted a staged approach to achieve the 91-rod bundle capacity and full transient capability of the loop. Although the design of the loop to the full 91 rod capacity and full transient capability will be completed, initially a Stage 1 CFTL will be constructed with only a 37-rod bundle capacity and limited transient capability. The scheduled completion of the Stage 1 CFTL near the end of FY 1981 remains unchanged from the preceding progress report.²

During CY 1979 the Title I design work for construction of the CFTL loop was completed, the Title II design for the mechanical and electrical engineering was nearly completed, and the Title II design for instrumentation and controls was initiated and accelerated. A new cost estimate and schedule were prepared.

Construction of site modifications and installation of the primary electric power supply and utilities were 90% completed and were ahead of schedule. Major components and equipment being purchased or fabricated include the test bundle power supply, heat exchanger HX-1, the heat removal blowers and ducts, the data acquisition system (DAS), helium flow control valves, mechanical flanges, and piping. Upon completion of the successful performance tests with the helium circulator prototype, two

additional circulators were ordered to provide, along with the prototype, the three circulators required for the CFTL. At the end of CY 1979, 70% of all procurement for Stage 1 CFTL had been initiated by requisitions.

A revision of the System Design Description for the CFTL was initiated to reflect the changes to the GCFR core and core assemblies. A Quality Assurance Plan for the CFTL Test Bundles (Document Q-11590-002-S-0) was prepared.

In 1979 the design of the loop piping, the test vessel, and the first test bundle, AGI, was impacted by several changes in the designs of the GCFR core and core assemblies. The direction of helium coolant in the core was changed from downflow to upflow, and lateral restraint of the core assemblies was added. Both the diameter and lengths of the fuel rods and ducts were increased also. All these changes were incorporated into the loop and bundle designs.

The development testing of the helium circulators and the performance testing of the vortex shedding flowmeter were completed. Development work continued on the use of x-ray imaging to observe test bundle movement within the test vessel.

The major analysis effort was devoted to the implementation of a modification of the COBRA-IV computational procedure supplied by General Atomic Company (GA). Efforts were directed toward modifying the coding so that it would execute properly on the IBM computers at ORNL and developing experience in the formulation of the proper set of input values to perform the desired calculations. Significant work was done on the heat transfer performance of artificially roughened surfaces. The CFTL bundle structural analysis has progressed to the use of a transfer-matrix method, and a functioning program was developed.

The design and development activities for the first CFTL test bundle, AGI, moved forward, and despite the changes made to the design of the GCFR core the bundle design was approximately three-fourths completed. All the bundle development work is proceeding to an orderly and satisfactory completion except for the fuel rod simulators (FRSs). Here a fabrication problem with maintaining good compaction density of the insulation and good heat transfer contact among the rod components has

led to random melting failures in the nickel end plug. The plug is near the joint between the central nickel electrode and the heating element, which is made of Kanthal A-1 alloy. Means for alleviating this problem are being pursued vigorously. Efforts were made to enhance the long-time reliability of the thermocouples in the FRS by reducing residual cold work from thermocouple fabrication to 1% and by reducing grain size to below the normal for the industry. An appropriate draw-and-heat-treatment schedule was developed to enhance thermocouple reliability.

1.2 IMPLEMENTATION OF ANALYTICAL PROCEDURES FOR PRETEST CALCULATIONS, DATA REDUCTION, AND DATA PRESENTATION — J. P. Sanders, K. W. Childs,* J. C. Conklin, W. G. Dodge, G. E. Giles,* S. A. Hodge, and C. A. Sady*

1.2.1 Introduction — J. P. Sanders

During CY 1979, several tasks that had been completed in previous years had to be revised. The first revision came after the change to the bundle AGI geometry. This change involved an increase in diameter and pitch of the fuel rod simulator (FRS or rod) and a change from six hanger rods in the peripheral rows to three rods in an interior row. A second revision occurred when the upflow design was chosen. This modification resulted in changes in the heated and the roughened lengths of the rods and in the length of the bundle duct.

The tasks that had been essentially completed but had to be redone because of these modifications were (1) calculation of the inlet flow distribution and pressure drop of the test bundle and (2) specification of the test section instrumentation. Changes in the location of proposed instrument sensors in the test section have delayed the preparation of a pseudodata tape for transmittal to General Atomic Company (GA). The current definition of the second bundle as a typical 61-rod blanket bundle has required the implementation of calculational procedures for spirally wound wire spacers.

A number of planning meetings for the analysis tasks plus short working visits during the year have been very beneficial to the analysis effort. The series began with a visit by GA personnel during February to

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discuss the analysis plans and objectives. This visit also afforded the opportunity to establish the agenda for a more comprehensive meeting on April 19 and 20. This joint analysis planning meeting at ORNL was attended by H. C. Hopkins, M. P. LaBar, G. Schlueter, and A. R. Veca of GA. As a result of presentations from ORNL staff members involved in the Blowdown Heat Transfer (BDHT) experiments, the analytical tasks planned by ORNL were significantly increased. It was agreed that ORNL will provide GA not only with the raw data from the test results but also with calculated surface cladding temperatures.

At the April meeting, G. Schlueter presented a comparison of Biot numbers for the CFTL FRS and for GCFR fuel rods at both normal operating and upset conditions. Since the calculated Biot numbers are sensitive to the values assumed for the contact coefficients for the different materials internal to the FRS, the possibility of evaluating the performance characteristics of a single FRS in an existing water loop is being considered. Also, according to the BDHT experience relating power transients in electrically heated FRSs with power transients in reactor fuel rods, it was decided that GA would supply ORNL with anticipated GCFR power transients and ORNL would determine the power programming necessary to simulate this transient in the FRS. Concomitant with this decision, GA agreed to provide material property values and thermal characteristics for GCFR fuel rods to ORNL.

We reviewed the planned use of analytical methods for the pretest calculation of anticipated values from test section instrumentation and the on-line data reduction and comparison to protect the bundle from unanticipated failures. This review indicated that the ORNL analysis efforts would make a significant contribution to the qualification of these analytical methods for subsequent licensing review. The importance of this aspect of the analysis effort led to further discussion at a meeting held in San Diego on August 9 and 10 concerning plans and a timetable leading to a demonstration and a commercial GCFR plant. Accordingly, it was agreed that the ORNL computational procedures must stay consistent with those at GA. To provide this assurance, it was agreed that listings of significant computational procedures should be exchanged at regular quarterly intervals.

A meeting was held at ORNL on September 12 with representatives of DOE-Washington and GA. Based on previous agreements concerning the division of responsibilities and task requirements, an outline of proposed ORNL CFTL Analysis Tasks was presented. This outline included all planned activities for the period of operation and data reduction for six bundles. The estimate was based on the assumption that analysis tasks would extend through FY 1985. An accompanying budget schedule begins with a stated available funding level of \$280 k for FY 1980 and continues with funding levels of \$420 k for FY 81 and 82 and funding levels of \$300 k, \$400 k, and \$350 k for fiscal years 83, 84, and 85, respectively. This schedule does not allow for further revision of completed tasks due to changes in bundle design or in specifications of the operating conditions.

The ORNL CFTL analysis effort benefited significantly from the fact that S. A. Hodge was working with the group during most of 1979 under sponsorship of the Oak Ridge Associated Universities (ORAU) as a Graduate Fellow from the University of Texas. The requirements of providing an appropriate thesis topic plus the support provided by ORAU allowed him to review in detail the analytical procedures used to correlate the performance characteristics of artificially roughened surfaces. His work resulted in not only a thorough review and evaluation of existing methods, but also a novel method of correlating available data. The work was published not only as a thesis but also as an ORNL report.³ As a vehicle for the application of this method, he revised the SAGAPO computational procedure and modified the procedure to represent the current AGI geometry.

J. C. Conklin attended two benchmark meetings in Europe. These meetings, designated BM-IV and BM-V, provided the bases for comparison of computational procedures for the evaluation of thermal-flow conditions in gas-cooled bundles with available experimental data. The first benchmark meeting was held in conjunction with a specialists meeting for experimenters who provided experimental data from gas-cooled bundles. Conklin presented three papers generated by the ORNL analysis work. These papers included the relationship developed by Hodge for isothermal flow past roughened rods, an evaluation of uncertainties associated with determining the external surface temperature of an FRS based on an internal thermocouple reading for the CFTL, and calculations performed for the CFTL bundle according to the procedure provided by GA.

The most substantial effort during CY 1979 has been the implementation of modifications to the COBRA-IV computational procedures supplied by GA. At the beginning of the year, work was proceeding on a version designated COBRA*GCFR, which had been transmitted in May 1978. Subsequently, a second transmission was made of a subprogram, CIRCON, designed to execute in conjunction with a modified COBRA*GCFR to modify the rod circumferential thermal gradients through the representation of azimuthal conduction of heat. This subprogram was transmitted in January 1979 and was made operational on the ORNL IBM computers by April. A copy of the modified COBRA*GCFR was requested and a listing on paper was received. Because of the extent of the modifications, a copy on magnetic tape was requested to ensure accurate implementation. Problems in getting the tape copy cleared at GA prevented its transmission. At this point, GA indicated that a completely revised version of COBRA*GCFR would soon be available that would incorporate the CIRCON calculation directly. A request was made in October, and the computer tape to fill that request had not been transmitted by the end of the year.

The major problems associated with the implementation of COBRA*GCFR at ORNL are related to the fact that the original COBRA-IV coding was developed on a CDC-7600 computer and modifications at GA were incorporated on a UNIVAC-1100 computer. A number of features associated with symbolic coding, input-output operations, and methods for storing values in computer memory on these two computers are not compatible with analogous operations of the IBM 360 computers at ORNL. It is therefore necessary to substitute procedures or coding to perform analogous operations. In addition, the number of equivalent significant decimal digits contained in a normal single-precision word on the three computers is not the same. The IBM carries about 7 significant digits, the UNIVAC carries about 8 digits, and the CDC carries about 14. Even though five significant digits would be sufficient for most output values, the iterative numerical techniques used in COBRA can result in differences in the third and fourth significant digits for calculated values. This has required modification of the program to execute in double precision on the IBM computer so that we can be certain that all computations are duplicated.

Since the original COBRA-IV code was capable of executing transient calculations, it was assumed at ORNL that this feature was retained in COBRA*GCFR. Furthermore, because a COBRA-IV version for light-water reactors was operational for transient problems on the IBM computers at ORNL, an attempt was made to exercise this segment of the coding on the IBM system. In addition, the transient procedures appeared, at first, to require the "roll" and "restart" features of the COBRA-IV code, and neither of these features worked satisfactorily at ORNL.

After significant effort, GA informed ORNL that the transient calculational procedure had not been previously exercised for a meaningful transient. It also appeared that neither of the two transient options permitted the execution of a meaningful transient case with a reasonable allocation of computational time. It was also found later that use of the "roll" and "restart" features was not required to execute a transient case. General Atomic is investigating alternate computational procedures or modifications of these procedures in COBRA. ORNL has suspended work on the transient calculational procedure; however, work is continuing for the implementation of the "roll" and "restart" features.

At the beginning of the year, effort in the area of structural analysis was devoted to the construction of appropriate models for an existing finite-element code, ADINA. Use of this general-purpose code required that some additional subroutines be written to model correctly salient features of the bundle structure. During the course of the year, it became apparent that this method would be inadequate to model all the interactions of the composite bundle. At this point, with the concurrence of GA, work was begun to develop a transfer-matrix method that would more easily and accurately model the interactions at the rod-spacer interface and at the spacer duct junction. By the end of the year, a computational procedure had been developed, and its effectiveness was shown to work for the AGI bundle. A listing of the procedure, which was written in the BASIC symbolic language for a Tektronix 4051 computer, was given to GA at a review meeting held in San Diego.

For the AGI geometry, the locations of thermocouples and pressure taps were specified for bundle AGI on the basis of general guidelines

based on the objectives of the analysis tasks. Measurements were indicated for critical locations that would indicate abnormal performance, which might result in unanticipated bundle failure, plus those locations that would provide the information required for confirmation of the computational procedures. An effort was made to provide redundant measurements at symmetric locations so that unusual readings could be properly attributed either to abnormal performance or to abnormal instrument function.

1.2.2 Application of $R(h^+)$ and $G(h^+)$ Functions — S. A. Hodge

Primary effort during 1979 was devoted to an extensive review and evaluation of the available information from the literature concerning the determination of friction factors and Stanton numbers for flow past artificially roughened surfaces. From this analysis, a new method for experimental determination of the $R(h^+)$ and $G(h^+)$ functions was developed. This work was performed by the writer at ORNL under sponsorship of ORAU as a Graduate Fellow from the University of Texas at Austin. Subsequently, this work was published as an ORNL report³ in addition to a dissertation at the University of Texas. The abstract of the ORNL report follows:

A critical review of the assumptions, theoretical foundations, and supporting experimental evidence for the analytical procedures in current use for evaluation of the effects of artificial surface roughening on friction factor and Stanton number is provided. Recommendations are given concerning the application of these procedures to rough rod bundles. A new method is demonstrated for determination of the slope and intercept of the universal logarithmic dimensionless velocity distribution law for fully rough flow past roughened surfaces without the need for experimental measurement of the velocity profile. The slope is shown to vary with the nature of the roughened surface and to deviate significantly from the slope for turbulent flow past smooth walls in some cases. It is further shown that the intercept, which is a boundary condition equivalent to the roughness parameter for friction, is independent of the width of the velocity profile. A similar method is developed for determination of the slope and intercept of the temperature distribution law, but additional experimental investigation is required before the efficacy of this application can be conclusively established.

In a related activity, the computer code SAGAPO/CFTL was developed to incorporate this improved methodology by modification of the steady-state code⁴ SAGAPO for application to the CFTL 37-rod bundle. The alterations to the original code include

1. Any dependence of the $R(h^+)$ and $G(h^+)$ functions upon the channel geometry was eliminated. This significantly reduced the required computation time.
2. Any dependence of the $R(h^+)$ function upon the wall-to-bulk temperature ratio was eliminated.
3. The structure of the code was adjusted as necessary to account for the larger diameter of the grid spacer support rods located within the CFTL bundle.
4. The code was adapted for calculation in a one-sixth symmetric sector of the CFTL bundle.
5. Printed output of the following was provided: pressures and rod wall and channel gas bulk temperatures at the axial distances from the bundle inlet corresponding to the thermocouple and pressure tap locations of the CFTL.

Sample calculations using SAGAPO/CFTL have been completed for the high-pressure normal-flow case, the high-pressure low-flow case, and the low-pressure low-flow case. Although the fine bundle structure employed by SAGAPO/CFTL produces a desirable very detailed analysis of the steady-state problem, this code does not have transient analysis capability. Furthermore, thermal radiation and the effect of rod circumferential conduction are not represented in the computational model.

1.2.3 Confidence Limits for Thermal Measurements in the CFTL Bundle - K. W. Childs* and J. C. Conklin

Work was initiated to calculate the confidence limits of the thermal measurements obtained from the rod thermocouples in CFTL test bundles. Qualification of computational methods for use in design and licensing requires that surface temperatures for the FRSs be determined from

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measurements made with internal thermocouples. This calculation requires accurate knowledge of the thermal properties of the constituents of the FRS plus thermal characteristics at the internal insulator-cladding interface, all as a function of temperature.

The results of a preliminary study of the thermal conditions in the CFTL rod are given in a paper⁵ prepared for presentation at the Fifth GCFR Heat Transfer Specialists Meeting sponsored by the OECD-NEA Coordinating Group on Gas-Cooled Fast Reactor Development. The report gives the results of a statistical evaluation of uncertainty limits for the cladding surface temperature at a maximum rod power of 38 kW as deduced from a measured internal thermocouple reading. The 95% confidence interval for the calculated surface temperature was found to be $\pm 45^{\circ}\text{C}$ at the midplane of the rod, which was the plane of maximum power generation. The thermal analysis was performed by using the ADINAT finite-element computational procedure.

A subsequent computation was based on a total power of 28 kW for the FRS and a 1.21 ratio of maximum-to-average axial power. The resulting thermal field is shown in Fig. 1.1.

Of the eight parameters that were considered in performing this statistical evaluation, the contact coefficient at the interface between the BN insulation and the cladding was found to be one of the more important. The uncertainty in the value of this coefficient, and therefore in the surface temperature, can be reduced by making experimental measurements using the FRS as manufactured. Also, this analysis considered only a two-dimensional heat conduction model. It is possible that a three-dimensional analysis, which represents the attachment of the extension of the thermocouple to the inside of the cladding, will indicate a smaller value for the uncertainty.

All the information used for this preliminary uncertainty study must be updated to reflect subsequent modifications in the rod dimensions. This information has been used to generate the necessary input data for the computer code ADINAT used for the uncertainty analysis. Further work is awaiting the completion of experimental tests on CFTL rod prototypes. These tests will supply more accurate information on the thermal properties

Fig. 1.1. Thermal Field ($^{\circ}\text{C}$) in a CFTL Fuel Rod Simulator at the Thermocouple Junction Using Nominal Parametric Values at the Midplane of the Rod with 28 kW Total Power and an Axial Power Ratio of 1.21.

of the constituents of the FRS plus thermal characteristics at the cladding internal surface. For the preliminary uncertainty study, the thermal properties of the constituents were based on information from the fabrication of an FRS at ORNL for the BDHT experimental loop. The thermal characteristics at the cladding internal surface were also estimated from the BDHT loop.

The uncertainty limits will be recalculated with the new dimensions, revised operating conditions, and the thermal characteristics resulting from the review.

1.2.4 Travel to Switzerland and West Germany — J. C. Conklin

Two visits during 1979 were made to the Kernforschungszentrum Karlsruhe (KfK), Karlsruhe, West Germany, and to the Eidgenössische Institut für Reaktorforschung (EIR), Würenlingen, Switzerland. The

purpose of the first trip was to attend the Fifth GCFR Heat Transfer Specialists meeting, sponsored by the OECD-NEA Coordinating Group on Gas-Cooled Reactor Development and to attend the Benchmark Calculation-IV meeting. The purpose of the second trip was to attend the Benchmark Calculation-V meeting.

Three papers were presented at the Fifth GCFR Heat Transfer Specialists Meeting, held at Würenlingen, Switzerland, on May 14 through 16, 1979. These papers dealt with characterization of the flow behavior past roughened surfaces, the current status of COBRA*GCFR at ORNL, and an error analysis of internal thermocouple measurements in the proposed FRSS for the CFTL. Much valuable information was obtained at the meeting from discussions with the international participants and from the papers provided.

The subsequent Benchmark-IV meeting compared calculated results of Swiss, German, British, and U.S. computer codes with four Swiss experiments. All codes agreed quite well with the experiment for internal channels in turbulent flow. However, discrepancies were observed in laminar flow, and the effects of fluid buoyancy and rod conduction were identified as areas requiring additional work. Also, the calculation of duct temperatures did not agree with experimental measurements because of differences in code methodology and the nonuniformity of the duct heat loss distribution. Information valuable to the evaluation of data from the CFTL was obtained in many discussions with the various participants.

The Benchmark Calculation-V meeting, held on December 13 and 14, 1979, at KfK, was a continuation of a series of meetings to compare the calculated results of computer codes with experimental results. The results of all codes compared favorably with experimental results for uniform power cases. However, the agreement between calculated and measured values for the duct wall temperatures and edge rod temperature was not as good as that for the interior rod temperature. Uncertainty in the amount of the local heat loss from the duct, as observed in Benchmark-IV, was cited as the source of these discrepancies. The CFTL will be instrumented to evaluate the local duct heat loss. Agreement for the nonuniform power

case was not as good as that for the uniform power case, and a parametric study will be necessary to resolve the differences. Rod thermal conduction and thermal radiation were further identified as important heat transfer mechanisms within the bundle.

During both visits, much information was obtained from EIR and KfK concerning their analytical efforts and experimental experience. Measurement techniques and error analysis were discussed in detail at both locations.

1.2.5 Implementation, Evaluation, and Development of Computational Procedures for Thermal-Flow Analysis - J. C. Conklin

1.2.5.1 TSPEC - Single-Channel Analysis Code - G. E. Giles*

The single-channel thermal-hydraulic analysis code TSPEC was developed⁶ at GA to provide preliminary information for the CFTL and GCFR bundles. ORNL implemented GA's TSPEC code on the IBM computer and then further modified it to produce a program designated as TSPECA. This version incorporates additional information in the program output and the heat transfer coefficient plus friction factor correlations contained in COBRA*GCFR. Results of TSPECA, TSPEC, and COBRA*GCFR calculations for models that were as consistent as possible were compared. This comparison indicated the need for an accurate multichannel steady-state and transient analysis technique. The above comparison, analysis of the operational limitation of the CFTL bundle, and temperature histories for the first 177 h of the proposed test program will be reported in an internal ORNL document. The temperature histories are being utilized in the estimation of the errors produced in thermocouples due to the time-at-temperature effects over the length of the thermocouple leads.

1.2.5.2 CIRCON - Circumferential Conduction Supplemental Analysis for COBRA*GCFR - J. C. Conklin and G. E. Giles*

The CIRCON program was developed to calculate the thermal conduction heat transfer in a rod from the COBRA*GCFR calculated heat flux. The original procedure was to execute CIRCON after a COBRA*GCFR analysis to

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determine the effect of thermal conduction on the temperature in a rod surrounded by channels with different gas temperatures. A copy of CIRCON was received from GA and implemented on the ORNL IBM computer. The computational expense of CIRCON was reduced 30% by further modifications in preparation for the coupling of the CIRCON and COBRA*GCFR analyses. A copy of the GA version of COBRA*GCFR that produces the information necessary for the CIRCON analysis was requested. A listing was sent; however, the modifications were not isolated to a small section of the code. A magnetic tape copy was then requested to ensure accurate implementation of the coupled technique. This technique for the circumferential heat conduction was superseded by the COBRA*BMV development; therefore, no tape copy was transmitted. Work on CIRCON was postponed until the BMV version is received.

1.2.5.3 Implementation of the May 22, 1978, Version of COBRA*GCFR — J. C. Conklin, G. E. Giles,* and C. A. Sady*

A copy of the GA COBRA*GCFR version that should be identical with the IBM version was requested September 1979 to ensure that an error had not been made in the modification of the code for the IBM computing system. The first tape was received in late October but was incorrectly written. A second tape was received December 15, 1979, and was successfully read. This version (THSD2566) was compared with the IBM version by using the program FILCOM. The differences found were due to (1) implementation on the IBM computer, (2) implementation of updates to the COBRA-IV code, and (3) implementation in the IBM version of a minor modification that GA previously transmitted to J. C. Conklin. It was decided that implementation of the THSD2566 version was not necessary since the IBM version incorporated more recent updates.

1.2.5.4 Investigation of the IBM Version of COBRA*GCFR — J. C. Conklin, G. E. Giles,* and C. A. Sady*

The multichannel, thermal-hydraulic analysis program COBRA*GCFR was adapted at GA for the GCFR from the COBRA-IV program originally developed for the water reactor program. The COBRA*GCFR code was implemented on the

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IBM computer at ORNL and appeared to execute steady-state analyses correctly. The areas that the IBM code did not appear to execute correctly were the roll, restart, and transient options.

The investigation of the transient option failure was initiated in August. A coding problem was identified and fixed to enable transient execution of the code. General Atomic was informed of this correction. Several transient and steady-state test cases were executed during this investigation, including a typical transient CFTL test case and the "fuel thermal model" test case supplied with the COBRA-IV program. The "fuel thermal model" steady-state and transient results did not compare well with the results from the CDC execution but compared fairly well with the IBM version of COBRA-IV. The CDC results (contained in the COBRA-IV manual) may not be for a current version of COBRA-IV. The discrepancies are being investigated. The CFTL results appeared reasonable, but no conclusion could be made since transient experimental data were not available.

The work done at ORNL on the IBM implementation of COBRA-IV indicated that a single-precision IBM version of COBRA-IV was not accurate enough for transient analysis because of the possible accumulation of round-off errors. Hence, COBRA*GCFR was modified to double precision for all floating point variables to increase the number of significant digits used. Comparison of the results from the same calculation performed in single- and double-precision versions revealed only slight differences for the steady-state analyses.

During the search for the cause of the transient option failure, several test cases were executed for the IBM version, and one failed for the steady-state calculations. This case was a slight modification of previous CFTL analysis, and the failure caused some concern that the IBM version was in error for the steady-state calculation. After consultation with Chandu Baxi of GA, a small reduction in the axial flow relaxation factor enabled the completion of the steady-state calculations. The results of these test cases compared very favorably with those from UNIVAC execution. This agreement for the steady-state analysis allows some

confidence in the IBM version for this analysis technique. The roll and restart options still fail to execute properly. The nature of the problems appear to depend on the subroutine order, program size, and input data. These problems may be symptoms of major program errors.

1.2.5.5 Working Visit to General Atomic, December 3 through 6, 1979 — G. E. Giles*

A working visit to GA accomplished the following items. We informed GA of the programming techniques that cause difficulties and delays for IBM implementation. Some COBRA*GCFR test cases that had been executed on the IBM version were executed on the GA UNIVAC with close agreement for the steady-state results. Modeling and numerical technique parameters were discussed to transfer some of GA's experience to ORNL. The status of the COBRA*GCFR transient technique development was discussed. A detailed report was prepared.

1.2.6 Tubesheet Thermal Analysis — K. W. Childs*

A thermal analysis of the tubesheet was completed during June. The purpose was to determine the maximum temperature reached in the tubesheet for different operating conditions and to determine the heat input rate from the tubesheet to the test bundle helium coolant. The HEATING5 thermal analysis code was used to calculate the temperature distribution in the FRSS and the surrounding tubesheet. These calculations were also required in accordance with normal quality assurance procedures as an independent confirmation of the design calculations performed by J. A. Seneker and reported⁷ elsewhere.

The current results agree well with those previously reported.

1.2.7 CFTL Structural Analysis — W. G. Dodge

Thermal gradients that exist in the CFTL test assemblies during operation will induce bending deformations in the FRSS. These deformations are restrained by the spacer grids and result in contact and corresponding friction forces at the rod-spacer grid interface. If these

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forces and deformation become excessive, rod bowing or lockup could develop and affect the thermal-flow performance of the assembly. The magnitudes of these forces and deformations are also a significant factor in establishing the performance of the corresponding GCFR assemblies under normal reactor operations. One of the primary purposes of the CFTL test program is to verify the structural adequacy of the GCFR core assemblies under thermal flow conditions simulating various GCFR operating conditions.

The applicability of various instrumentation for monitoring the structural performance of the CFTL assemblies has been investigated. The only suitable method for monitoring the deformations of the assemblies during operation is the use of x-ray techniques to monitor the displacement of appropriate markers implanted to the structure. A plan for placement of platinum markers in the 37-rod bundle was prepared and distributed for comment. Under this plan, bowing of the FRSSs would be determined by observing markers placed within the rods approximately midway between adjacent spacer grids. Bowing of the bundle would be determined through the use of markers placed at appropriate points on the outer duct. To establish a reference for measuring displacements, markers will also be placed at the point of intersection of each of the imaging rays and the vessel wall. The grid webs are too thin to have markers embedded in them, so we will not be able to obtain quantitative information on spacer grid deformation.

Because of the limited nature of the instrumentation available for monitoring the structural performance of the test assemblies, it will be necessary to rely on analytical methods to complement the experimental evaluation of their performance. The analysis of a structure having the complexity of the fuel test bundle is difficult, and no previously published results could be found to provide insight into an appropriate approach. At the beginning of the CFTL structural analysis effort at ORNL, two methods of analysis were considered: (1) a method using modified versions of existing general-purpose finite-element codes and (2) a specialized code based on the transfer matrix method. The primary difficulties associated with the use of existing finite-element codes are the high computational cost and the difficulty of interfacing these codes

with existing thermal analysis codes. The primary difficulties associated with the use of the transfer matrix method are the requirements for developing a specialized computer code and the difficulty of modeling the large number of statistically indeterminate interactions. The method proposed for handling these interactions in the transfer matrix approach was cumbersome and involved iterations at several levels; thus, the decision was then made to proceed with attempts to utilize finite-element methods of analysis.

A finite-element model of a 37-rod assembly was constructed. It assumes rigid spacer grids and neglects frictional interaction between the FRSs and spacer grids. Although this model does not incorporate the effect of frictional interaction between the rods and spacers, it does provide a means for determining the magnitude of the contact forces and gives insight into the effort required to perform a coupled analysis of the assembly using a general-purpose finite-element code. The finite-element computer program ADINA⁸ was modified to allow the tying of structural degrees of freedom required to model the in-plane rigidity of the spacer grid. A preprocessor program was developed to compute the pseudonodal loads corresponding to the temperatures and temperature gradients existing in the assembly. A specialized mesh generator program was written to assist in preparing the input data for the ADINA code, and development was begun on a specialized postprocessor program to extract the appropriate information from the results.

Two analyses were run corresponding to two different configurations of the 37-rod bundle. The thermal loading conditions for these analyses were constructed to approximate the temperatures and thermal gradients that might exist in the bundle during a maximum power skew. Although both analyses appeared to be successful, it became evident that considerable investment of manpower and computer resources would be required to utilize this method for making calculations during testing of the bundle and in the design evaluation of GCFR assemblies.

During the latter part of 1979, an improved procedure was developed for representing the interaction conditions in the analysis of a fuel bundle using the transfer matrix technique. The procedure directly

utilizes the known interaction conditions to eliminate the constraint unknowns from the analysis as they are encountered. With this procedure, three equations in three unknowns constitute the largest system of equations that must be solved. The only iterations involved are those associated with satisfying the nonlinear frictional interaction at the interface of a rod with a spacer grid and at the duct constraint interface. To test the effectiveness of the method, a program to model the 37-rod bundle was developed for the Tektronix-4051 microprocessor terminal. Using this program, we have shown the solution algorithm to be effective for all combinations of sliding, locked, and buckled rods. A presentation of the method has been made to GA, and a report documenting the technique is being prepared. This new technique will provide a viable means for performing structural analysis of the CFTL and GCFR fuel assemblies.

1.2.8 Specification of Instrumentation for the AGI Bundle — J. P. Sanders

Basic criteria had been established at ORNL for the placement of instrumentation in the test sections of previous bundle geometries. The same criteria were retained as the basis for the location of bundle instrumentation for the AGI geometry. The criteria are based directly on the fundamental objectives of the analysis effort, which are (1) to protect the bundle from unanticipated failures during testing and (2) to provide experimental data converted to engineering units (as opposed to millivolt reading) to GA for use in the verification of computational procedures.

During the year at a joint meeting, GA and ORNL personnel who are engaged in the analysis effort recognized that it would be desirable for the ORNL analysis task to provide not only the internal FRS temperatures but also calculated rod surface temperatures and associated confidence limits. This calculation depends upon experimental values determined for the FRS in testing before bundle assembly and during the programmatic testing procedure. This additional task did not affect the basic criteria for the location of bundle instrumentation.

1.2.8.1 Internal FRS Thermocouple Locations — J. P. Sanders

The decision was made at ORNL and accepted by GA to include six internal thermocouples at equal intervals around the interior cladding surface of each rod. Also, GA recommended that the thermocouples be grouped in diametrically opposed pairs to measure the thermal gradients that might induce rod bowing. This recommendation was supported by the structural analysis study at ORNL, and it has been incorporated as an additional criterion for the thermocouple placement.

To enhance the thermal measurements that can be obtained at the required number of axial locations and at the same time minimize the impact of trying to accommodate an indefinitely large number of types of FRS designs, a limited number of diametrically opposed pairs of thermocouples were combined with individual thermocouples at different axial positions to establish four different thermocouple configurations in the FRS. By rotating these four thermocouple configurations as they are placed in the bundle, measurements at the desired locations can be obtained. The locations that were chosen represented symmetrical positions with respect to either the bundle or a subchannel. A subchannel designates that flow area defined by three adjacent rods in the interior of the bundle or by two rods and the adjacent hexagonal duct wall at the bundle edge.

Symmetrical thermocouple locations on a subchannel will allow differentiation between thermal effects in the FRS performance or in a flow maldistribution due to rod bowing. Within an FRS, high readings on the internal thermocouples can be generated simply by an increase in the contact resistance between the thermocouple and the sheath due to differences in thermal expansion. Symmetrical locations within the bundle can identify anomalies in flow distributions or variations that might be associated with changes in the flow regimes.

1.2.8.2 Gas-Stream Thermocouples — J. P. Sanders

In conjunction with the thermocouples located symmetrically with respect to the subchannels, a limited number of thermocouples are located in the gas stream on selected grid spacers. In the heated section, these thermocouples provide a secondary evaluation of the thermal characteristics

of the performance of the internal FRS thermocouples. The gas-stream thermocouples downstream from the heated section provide a means of establishing the effect of inter-subchannel mixing on the transport of heat across the bundle.

1.2.8.3 Pressure Tap Locations — J. P. Sanders

It was assumed that pressure gradients across the bundle would not be significant with the exception of the entrance segment, in which flow enters radially and redistributes into an axial flow pattern. The spacer grids also create a temporary radial redistribution of flow due to the large flow blockages in the edge channels. As a consequence, pressure taps were placed at the center of the flats of the hex duct, and pressure taps for determining pressure differentials were placed on symmetrical faces. It will be noted in Fig. 1.2 that the positioning of three support

Fig. 1.2. Location of Pressure Taps at Midpoints of Sides of Hex Duct (Flow Direction Is into the Page) in CFTL Bundle AG1.

rods within the hexagonal channel leaves adjacent and opposing hex faces in unsymmetric relationships. In the figure, faces A, C, and E are symmetric with each other and faces B, D, and F constitute a second group.

The pressure taps are located axially at points so that various pressure loss effects can be isolated and characterized. One pressure tap penetrates the bundle tubesheet to obtain the inlet bundle static pressure. This pressure reading must be corrected for the change in direction and velocity as the flow enters the bundle. Other pressure taps are positioned to determine the pressure drop due to spacer grids in both the smooth and rough sections. Because of the relatively small pressure differentials anticipated, the spacer grid losses are associated with a selected segment of bundle. Determination of the pressure differential over a section of similar length that does not contain a spacer will allow the spacer loss to be determined as a difference between the two values. Similar segments in the inlet and outlet sections will allow the effect of changing coolant viscosity and density to be measured.

There will be an entrance effect on the pressure-loss relationship at the transition from a smooth to roughened rod surface. There will also be an entrance effect at the transition from unheated rod to heated rod. Unfortunately, these effects cannot be isolated physically since the AG1 design separates these two transition points by only 10 mm. Tests can be conducted at heat loads varying from zero to full power, and then the two entrance effects can be isolated.

1.2.8.4 Thermocouples External to the Bundle — J. P. Sanders

The conclusions of recent benchmark meetings have indicated that a failure to represent accurately the heat loss from the bundle shroud in either the experimental measurements or the calculations may account for an observed lack of agreement between the respective values. To provide the required information for the CFTL bundles, thermocouples have been designated at strategic locations on the outer surface of the hex duct and the inner surface of the annular shroud that surrounds the duct. These thermocouples are placed at the midpoint of the heated length, where the heat generation is at a maximum, and along the outlet segment, where gas temperatures are highest.

1.2.8.5 Mean Outlet Gas Temperature Measurement — J. P. Sanders

As a check on all thermal and flow measurements, it is necessary to complete a heat balance around the test section. The inlet flow and temperature are measured, the heat input from the FRS is determined from voltage measurements and the known internal resistance of the rod, and the heat loss through the hex duct wall can be established from the temperature measurements described in Sect. 1.2.8.4. The remaining information needed to close the heat balance is a measure of the mixed mean outlet temperature.

A proposal was made during the year to incorporate a mixing device in the outlet segment of the hex duct and then measure the mixed mean temperature. Subsequently, it was concluded that design and installation of the flow mixer would present both fabrication problems and a significant expenditure. The decision was then made to utilize the naturally occurring mixing that exists in the unheated outlet blanket segment of the bundle. A five-prong thermocouple rake located at the duct outlet, downstream of the bundle FRSs, would be used to measure the outlet temperature and to establish the occurrence of complete mixing.

1.3 DESIGN AND CONSTRUCTION OF THE LOOP — W. R. Huntley and C. W. Collins

The design and construction activities for the CFTL loop continued under the staged approach to allow initial CFTL operation to begin at the lowest practical cost. The Stage 1 CFTL, in which certain components are deferred to Stage 2, will provide an initial 37-rod bundle capacity and limited transient capability. The design of the Stage 2 CFTL, which includes the Stage 1, is being completed to provide full 91-rod bundle capacity and full transient capability for the loop.

During CY 1979 the loop design was impacted by changes made to the GCPR core and core assemblies. The direction of helium coolant through the core was changed from downflow to upflow, and the lengths of the fuel rods and assembly ducts were increased. The resultant design changes to the piping system and the test vessel were substantially accomplished.

The Title II design for the mechanical and electrical engineering was nearly completed during the year, and the Title II design for

instrumentation and controls was accelerated. A new cost estimate and schedule were prepared. The estimated completion date for the construction and acceptance testing for the Stage 1 CFTL is the latter part of FY 1981.

Site modifications performed by the cost-plus-award-fee (CPAF) contractor were about 90% completed and were ahead of schedule. This work included the demolition activities needed to clear the area, the installation of air-cooling equipment for the electrical equipment, the construction of the loop enclosure and the control room with all utility services, the installation of the primary electric power distribution system, the procurement and assembly of the air-cooling system for the loop heat sinks, and the installation of the helium supply system.

Procurement began for 70% of the equipment needed for the Stage 1 CFTL. The major mechanical equipment being fabricated or on order includes heat exchanger HK-1, three helium circulators, helium flow control valves, hydraulic actuators for the flow control valves, mechanical flanges, pipe, plate, and fittings. The major electrical equipment on order is the first of seven zone power supplies; the six additional supplies for Stage 1 will be purchased as soon as the successful operation of the first supply is demonstrated. Two variable-frequency power supplies for the helium circulators were ordered in addition to the prototypic supply presently in use at the circulator manufactory. Procurement of the data acquisition equipment was nearly completed, and the system was undergoing acceptance testing. Successful evaluation tests of a vortex shedding flowmeter resulted in the decision to purchase these meters in place of the reference design venturi meters.

1.3.1 Mechanical Equipment and Systems — C. W. Collins

Significant progress was made in design, fabrication, and procurement of mechanical equipment and systems. Changes to an upflow GCFR core and longer GCFR core assemblies impacted design and schedule of the test vessel and loop piping.

Design was completed on the two heat exchangers and the fabrication of heat exchanger 1 was substantially completed. Heat exchanger 2 will be fabricated in Stage 2 of the program. All components of the cooling-air system were received and installed.

Piping changes were made to accommodate the upflow core, and all piping drawings are scheduled to be completed by April 1, 1980. Nearly all the piping and fittings are ordered and much of the order has been received.

The helium supply system piping was installed. The design of the helium evacuation and impurities systems will be completed in FY 1980. The Helium Purification System Specification was completed and the procurement will be made in Stage 2.

The test vessel was modified to accommodate the upflow core and longer bundle. The middle portion of the shell was changed from stainless steel to a stronger material, Inconel 625, to permit a thinner wall to improve the radiography for the structurally related measurements. Design is scheduled for completion on April 1, 1980. Major components were ordered and some received.

Design and construction of all building facilities were completed except for a few minor items.

1.3.1.1 Heat-Removal Equipment and Systems — C. W. Collins and W. R. Clark

The helium system heat removal requires two separate systems similar in design but considerably different in size. The mechanical portion of the main heat-removal system consists of a helium-to-air heat exchanger (HX-1) sized to remove 4.4 MW of test bundle and gas-flow frictional power and an accompanying air-handling system of 35.4 m³/s (75,000 cfm) capacity comprising a centrifugal fan, a variable-speed drive, dampers, and ductwork. The second system has a smaller capacity [0.4-MW heat exchanger, 5.66-m³/s (12,000-cfm) air-handling system] and is intended to remove mainly the circulator power input to the loop helium inventory during rapid transient operation.

The heat exchanger design was modified to install an air-flow baffle plate. This resulted from concern about possible acoustic resonance of the heat exchange tubes with a transverse acoustic standing wave in the rectangular cooling enclosure. A similar baffle solved a resonance problem in a large air-cooled heat exchanger on another experiment; thus, we believe that this modification will reduce the possibility of acoustic resonance in HX-1.

Following the examination and approval⁹ of the HX-1 design and supporting analyses by the CFTL Design Review Team, project approvals were obtained and construction drawings for HX-1 issued. Fabrication was begun in the ORNL shop, with some initial development work being required to perfect procedures for extruding the outlets on the headers and for bending the tubes within *ASME Boiler and Pressure Vessel Code* flattening and thinning tolerances. These procedures were successfully demonstrated, and fabrication of HX-1 is about 90% complete.

The engineering specification, design calculations (including heat-transfer performance, stress analysis, and pressure drop for both the air and helium sides), and detailed drawings have been completed for the smaller heat exchanger (HX-2). The design was evaluated and approved, with minor revisions, by the CFTL Design Review Team. However, since HX-2 is not a part of the Stage 1 CFTL, its fabrication and installation will be deferred.

Some revisions were required to the drawings of both the large and small cooling-air systems as more detailed information regarding such equipment as the centrifugal fans and dampers was received from the manufacturers. After completion of these revisions and final approval of the design, the drawings were issued for construction. Also approved and issued was the design package prepared by UCC-ND Engineering for such system components as the structural supports for the ductwork and equipment, air filter banks and their enclosures, etc. These items are discussed in more detail in Sect. 1.3.1.7, Building Facilities. Both the large and small cooling-air systems are under construction and will be completed (except for a portion of the small system ductwork to be deferred to avoid interference with the later installation of equipment) as a part of the Stage 1 CFTL while the necessary equipment and personnel are on site.

1.3.1.2 Piping and Equipment — W. R. Clark

Detailed design¹⁰ of the CFTL primary piping and full-flow helium filter was performed in CY 1979. Design activities for the circulators, control valves, flowmeters, and test vessel, which are also part of the helium circulation system, are discussed elsewhere in this report.

Some modifications to the conceptual piping layout were required by the change to an upflow core and the lengthening of the test vessel; however, these were accomplished without relocating the major equipment. An isometric sketch of the current configuration is shown in Fig. 1.3. Stress analysis of the piping has been completed by using the MEC-21 piping flexibility computer program¹¹ to determine stresses due to thermal expansion and weight loading. Loop pressure drop has been evaluated, and a dynamic analysis of the piping during loop blowdown has been conducted. All calculations indicate that the current layout is acceptable for the specified service conditions. Piping support hangers have been located according to the stress analysis results, and the design of the support structure for the hangers is complete.

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Fig. 1.3. Helium Circulation System for the GCFR-CFTL.

Since the piping is to meet the intent of the *ASME Boiler and Pressure Vessel Code* for Sect. III, Class 2 components, the procedures in the ORNL Controlled Manufacturing Manual must be followed throughout design, fabrication, and installation. The piping will be shop-fabricated into subassemblies with suggestions from the personnel responsible for the field fabrication used to simplify that task as much as possible. Procedures to ensure that acceptable quality will be maintained during the field welding and installation are being implemented.

The pertinent calculations and drawings will be distributed for evaluation and approval by the CFTL Design Review Team in February 1980. Most of the pipe and fittings for the primary system have been procured.

The helium vent system includes three flow paths for helium to leave the system. One path ensures overpressure protection and includes a pressure relief valve downstream from a rupture disk to prevent unnecessary helium leakage through the valve. Analysis has shown that loop overpressure due to overheating will not be a problem but, rather, that the system should be designed to prevent overpressure during helium filling and makeup activities. Thus the vent system has been designed for this condition. The second path includes a double rupture disk in series with an orifice to control pressure during depressurization transients. A system similar to those that operate successfully on other experimental loops will incorporate a plenum between the two disks filled with high-pressure gas and an explosive valve to blow the rupture disks and initiate the depressurization. The third path contains a needle valve for bleeding small amounts of helium to the atmosphere to control pressure during high-power operation. This is necessary since the increased temperature of helium volumes in the test vessel outlet plenum, hot-leg pipe, and hot end of HX-1 may increase the loop pressure at higher power tests. Detailed design work on the vent system has continued, with completion scheduled in early 1980.

Detailed design drawings for the helium filter are complete and ready for final project review and approval. The pipe, fittings, and filter tubes necessary for fabrication have been procured.

1.3.1.3 Helium Circulator Prototype — H. C. Young

At the beginning of CY 1979, bench testing¹² of the rotary assembly for the circulator prototype had been satisfactorily completed by the manufacturer, Mechanical Technology Inc. (MTI), in ambient air up to the 24,000 rpm maximum operating speed. Early in the first quarter of 1979, the rotary assembly was inserted into the circulator pressure vessel that had been installed earlier into the test loop, and shakedown operation started. Initially, helium leaks in the test loop were a problem. Replacement of a static seal in the bonnet of the flow control valve manufactured by Valtek corrected the last serious leak. This is a bellows valve that has a gasketed static seal at the body-to-bonnet joint. The circulation pressure vessel design provided for seal welding the vessel head to the vessel flange to make a fully hermetic seal. A metal O-ring was also provided between the vessel flange faces for temporary sealing during performance testing. Although the temperature at the seal region is below 100°C, the metal O-ring was unsatisfactory and was replaced by a Teflon Omniseal manufactured by Fluorocarbon Mechanical Seal Division. Acceptable leak rates were obtained with the Omniseal, so we do not plan to make the seal weld even for the installation of the circulators in the CFTL.

All loop pressure and flow measuring instrumentation was calibrated in place at MTI by General Electric Instrument Services in February 1979.

Testing of the circulator to determine that the performance met our specified conditions was completed, and a 100-h operation test at design conditions was completed by the end of March 1979. The performance tests were witnessed by ORNL personnel. The circulator operated very satisfactorily.

The aerodynamic design provided the specified head-flow performance at somewhat less than design speed. The performance test results are shown in Figs. 1.4 and 1.5. The effectiveness of the design of the motor cooling system provides motor capability well beyond the originally specified operating conditions. The Class C stator insulation has a continuous operating temperature rating of 220°C (428°F), while the maximum measured stator temperature was about 132°C (270°F).

9.0 MPa DISCHARGE PRESSURE; 345°C DISCHARGE TEMPERATURE

Fig. 1.4. Head-Flow Performance of CFTL Helium Circulator Prototype at 9.0 MPa (1300 psi) and 345°C (650°F) Discharge.

10.3 MPa DISCHARGE PRESSURE ; 345°C DISCHARGE TEMPERATURE

Fig. 1.5. Head-Flow Performance of CFTL Helium Circulator Prototype at 10.3 MPa (1500 psi) and 345°C (650°F) Discharge.

The journal bearings and thrust bearing operated satisfactorily from 0.10 to 10 MPa (15 to 1500 psia) at shaft speeds from 3000 to 22,000 rpm. The bearing film clearances were read out on oscilloscopes and provided clear indication of satisfactory bearing operation.

Circulator coastdown curves from design speed at 9.0 and 10.3 MPa (1300 and 1500 psia) were obtained. Starting from 22,000 rpm at 10.3 MPa, the coastdown time to zero speed was 96 s. The rotor moment of inertia is 0.123 kg m^2 ($420 \text{ lb}_m \text{ in.}^2$).

Depressurization tests were conducted at design speed, 21,150 rpm, from initial test loop helium pressures of 4.1, 5.5, 8.2, and two tests at 10.3 MPa (600, 800, 1200, and 1500 psia) to 0.1 MPa (1 atm). The maximum rate of depressurization was 2.0 MPa/s (300 psi/s). Recordings of thrust bearing clearance indicated that the thrust faces did not contact during any of the depressurization tests.

During depressurization tests the helium pressure in the motor cavity must be equalized with the rapidly decreasing impeller inlet pressure to prevent overloading the thrust bearing. This pressure equalization is accomplished through reed-type check valves located in the impeller back-plate assembly; see Fig. 1.6.

Although the specified helium temperature was 343°C (650°F) at the circulator outlet, the circulator was also operated at a higher helium temperature of 400°C (750°F) at 9.0 MPa (1300 psia) and 21,000 rpm for 1.5 h and at 10.3 MPa (1500 psia) at 21,100 rpm for 2 h on the design flow resistance line. The stator winding reached a maximum temperature of only 132°C (270°F).

At the completion of the performance testing and a 100-h endurance test, the circulator was disassembled for post-test examination. After 76 starts and stops and over 154 h running time the bearing surfaces were in excellent condition. Each journal surface had a shiny line opposite the bearing pivot point; however, MTI said that was normal for all pivoted pad gas-lubricated bearings. The thrust faces showed no indication of rubbing. This reinforced earlier measurements obtained from the thrust bearing clearance sensors, which indicated that the thrust faces did not touch during the depressurization tests.

Fig. 1.6. Impeller Back Plate Assembly of CFTL Helium Circulator Prototype Showing Reed-Type Check Valves for Equalizing Helium Pressure Between Motor Cavity and Impeller Discharge During Rapid Depressurization. Photograph by Mechanical Technology Incorporated.

All parts of the circulator are reusable; however, some minor modifications are necessary to correct potential problems.

Axial gall lines were visible on the piston ring surfaces and mating surface at the impeller inlet cone; MTI smoothed these surfaces and applied a molybdenum disulfide coating with a subsequent bake.

A sootlike deposit covered the colder surfaces in the motor cavity. This deposit is believed to be volatile material from the electrical insulation in the motor stator. Although helium samples were taken at 25-h intervals during the 100-h endurance test, the volatile material apparently condensed out in the motor cavity and did not reach the sample tubes. The soot was not considered to be detrimental to circulator operation; however, additional bakeout of the insulation in the motor stators for the production circulators will be performed.

The performance test report from MTI; reproducibles of the circulator drawings, including as-built revisions; latest revisions of the specifications for the circulator motor, pressure vessel, and motor cooling vessel assembly; and the report on post-test examination of the circulator were received by UCC-ND Purchasing Division on April 16, 1979. This completed

all the specified requirements for engineering acceptance of the circulator prototype at MTI. The total cost of design, fabrication, and performance testing for the CFTL helium circulator prototype was approximately \$1,100,000.

Performance testing of the circulator prototype showed that the motor cooling system and the Class C insulation on the motor stator winding will permit the circulator drive motor to develop substantially more power than the circulator specification required. We requested MTI to provide a proposal to obtain head-flow performance data up to full circulator motor capability as limited by either a motor hot spot temperature of 190°C (375°F), the motor saturation limit, or the current limit of the variable frequency power supply. The modifications required to perform this supplemental testing include replacing the 9.5-mm-diam (0.375-in.) solid copper conductors in the Ceramaseal feedthroughs for the motor power leads with 12.7-mm-diam (0.50-in.) conductors, installing larger power cables from the power supply to the circulator, replacing the terminal blocks at the outlet of the variable-frequency power supply, and reworking the valve plug in the flow control valve to permit achieving higher test loop helium flow. A vaneless diffuser will be tested in an attempt to reduce the surge point at design speed from 65% to below 50% of design flow.

A CPAF contract with MTI for performing these supplementary tests was issued July 1979. It was intended that MTI return the Ceramaseal power feedthrough to Ceramaseal Inc. to increase conductor size while machine removing the seal weld on the feedthrough. The ceramic portion was broken and an entirely new feedthrough was ordered. Lateness of delivery of the new feedthrough delayed the supplemental test schedule. The Ceramaseal was finally delivered in December 1979. All modifications required to perform the supplemental testing have been completed, and the testing is scheduled for January 1980.

1.3.1.4 Production Circulator Activities — H. C. Young and T. B. Conley

Three circulators operating in series are required in the CFTL. Originally we planned to purchase three production circulators and to

have the circulator prototype serve as a spare. Because of funding limitations, the circulator prototype will be installed in the CFTL, and only two production circulators were ordered.

The higher than estimated costs for the design (Phase I) and the fabrication and development testing (Phase II) of the CPAF contract for the circulator prototype led DOE to suggest that UCC-ND consider open bidding for the two production circulators. However, manufacture of the production circulators had been included as Phase III in the original subcontract for the circulator prototype. DOE agreed that good faith negotiations to arrive at a fixed-price contract with MTI for the production circulators should be continued, based on certified cost and pricing data for the circulator prototype verified by cognizant auditors.

The UCC-ND Purchasing Division was requested to initiate negotiations with MTI in late January 1979. Management of CFTL visited MTI in March 1979 to discuss negotiation of the fixed price contract for the production circulators. The UCC-ND will provide the pressure vessels,¹² motor cooling vessels, motor stators, motor rotors, and Ceramaseal feedthroughs for electrical power and instrumentation leads.

From dates for delivery of circulator components to be supplied by UCC-ND, MTI indicated that it could deliver the two production circulators by November 1980. However, MTI indicated that the negotiations for the fixed price contract would need to be completed and the approved contract placed by May 1, 1979.

The UCC-ND Purchasing Division advised that auditing MTI's certified cost and pricing data and final approval of the formal contract would take several months. As soon as MTI's certified cost and pricing data were received, a letter subcontract was issued to MTI in May 1979. This subcontract permitted and limited MTI to order circulator parts. Auditing and price negotiations continued throughout the summer, and the formal fixed price contract was officially placed with MTI in mid-September 1979.

Even with the advance ordering of material permitted by letter subcontract, MTI advised us in mid-September 1979 that the shipping date for the production circulators would be delayed two months from

November 1980 to January 1981. Meanwhile, difficulties in bidding and fabricating the motor rotors delayed our promised delivery to MTI from November 1, 1979, to February 1980.

Attempts to obtain bids for fabricating the motors were started in August 1978. Only one bid was received, and the bidder was awarded a contract for two motor stators, which were delivered in September 1979. The UCC-ND Purchasing Division decided that the single bidder did not have sufficient financial strength to handle the rotors also. A new request for proposal was issued to 14 specialty machine shops for fabrication of two rotors with a bid closing date of February 28, 1979. Proposals for rotor fabrication were also solicited from the X-10 and Y-12 fabrication shops at UCC-ND.

During this bidding period several questions regarding tolerances and assembly techniques on the MTI motor rotor drawings were raised. We asked MTI to clarify these questions and rework the rotor drawings. All bidders were notified that the bidding was temporarily halted until revised drawings could be issued.

Mechanical Technology Inc. had purchased the motor design from General Electric (GE) and had redrawn and condensed the details from seven of the original GE rotor drawings onto two MTI drawings; in several areas the information on the two sets of drawings was inconsistent.

Revised drawings of the motor rotor were prepared by MTI and approved by ORNL, and rotor bidding was resumed. A new bid closing date of April 5, 1979, was set.

All 14 outside bidders plus the X-10 shop declined to bid. The Y-12 shops provided a best effort cost estimate, and the task was assigned to them.

The original completion date set by Y-12 for the two rotors was November 5, 1979. Problems in procurement of the beryllium copper conductor bars and end rings, heat-treating of the AISI 4340 shaft material, precision gun drilling, and induction brazing of the beryllium copper parts have delayed the estimated completion date of the rotors to February 1980.

A subcontract for the pressure vessels was awarded to Ionics, Inc., Bridgeville, Pennsylvania. Delivery was originally scheduled for

March 1, 1980; however, recent difficulties encountered by Ionics in subcontracting the machining of the motor cooling jacket indicate that the delivery may be as much as two months late.

Three sets of three types of Ceramaseal feedthroughs required to provide a gastight exit from the circulator pressure vessels for the electrical power leads, the leads from the bearing capacitance probes, and the internal thermocouples were ordered from Ceramaseal in January 1979 with delivery expected in September 1979. All of two types of Ceramaseal have been delivered; however, Ceramaseal has encountered cracking problems with the third type (for the capacitance probe leads), and the most optimistic delivery date is mid-March 1980.

The production circulator contract with MTI includes options to purchase from one to three additional circulators as well as a spare circulator assembly. We are developing plans to order one complete spare internal assembly plus some minimum of spare parts.

One complete spare circulator assembly consists of all circulator parts except the pressure vessel and mounting base. The spare circulator assembly would be fabricated, assembled, and checked or tested by MTI and then safely stored near the CFTL site. If a production circulator in the CFTL should fail, the complete defective circulator assembly would be withdrawn from the circulator pressure vessel by means of the permanently installed circulator base assembly fixture and then removed with suitable slings and an overhead hoist. The pressure vessel would remain in place in the CFTL. The complete spare circulator assembly would then be inserted into the open pressure vessel, and the CFTL could be returned to operation in a much shorter time than it would take to replace parts in the failed assembly. The failed assembly could then be disassembled and repaired.

1.3.1.5 Evacuation, Helium Supply, Purification, and Impurity Injection — W. R. Clark

The loop evacuation system, helium supply system, helium purification system, and gaseous impurities injection system are auxiliary to the primary helium circulation system but are necessary for the successful completion of the test program.

The loop evacuation system is required to remove atmospheric air from the loop piping and components before the loop is filled with helium. Design calculations have been completed to estimate the system resistance to pumpdown and the necessary level of evacuation pressure so that, when the loop is filled with helium, contaminants from the residual air will not exceed allowable levels. Based on these calculations, a mechanical rotary-type vacuum pump with an ultimate pressure capability of 6.7 mPa and a pumping speed of 0.025 m³/s has been selected. The 0.1-m-diam (4-in.) connecting pipe will be kept as short as possible (about 2 m) to reduce system resistance. The isolating valve will be a full-bore ball valve for the same reason and will be located an adequate distance from the primary piping to avoid the need for a valve capable of operating at the piping design temperature of 399°C. A valve with Viton seats capable of operation to approximately 150°C has been identified.

The helium supply system consists of a helium fill subsystem separate from the helium makeup subsystem. The helium fill subsystem consists of a large supply trailer to be parked outdoors to supply helium to the pressure-control station. The final design of the makeup subsystem requires two banks of standard-size helium cylinders located near the experimental loop. Either bank can be connected to the loop through the pressure control station. The helium supply system up to the pressure control station is similar to compressed gas systems normally used in standard industrial practice. Design of the system has been completed and approved, except for the pressure-control station to be supplied later by the Instrumentation and Controls Division. Fabrication and installation of the system by the CPAF contractor are approximately 95% complete.

specification for the helium purification system has been prepared but has not been circulated for final project approval since the procurement of this item is deferred to Stage 2. The detail design of the gaseous impurities injection system has been delayed until the first quarter of CY 1980. The deferrals of these systems do not affect the construction schedule of the Stage 1 CFTL.

1.3.1.6 Test Vessel — W. R. Clark

The CFTL test vessel (Fig. 1.7) is required to provide a pressure enclosure of sufficient size to accommodate all anticipated test bundles.

Fig. 1.7. The CFTL Test Vessel with a 37-Rod Test Bundle Installed.

Other design criteria include providing a means for moderating the temperature of the extremely hot helium flow before it is discharged into the piping and heat exchanger and providing as suitable a facility as possible to accommodate the planned measurement of bundle motion during operation with an x-ray imaging system. A conceptual design¹³ for such a vessel was largely completed in 1978. As the detailed design progressed in 1979, however, several revisions of the preliminary design configuration were required to accommodate changes to the GCFR core caused by the convergence of German and American GCFR designs and from engineering decisions made as the design became more fully developed.

The decision that the GCFR will have an upflow core required, essentially, that the original test vessel design be inverted. The maximum test bundle length was increased to 4.5 m, which resulted in an increase in vessel length of approximately 2 m (80 in.). As an additional result of the GCFR changes, the plan for handling the test vessel and bundle has been extensively revised. Originally, the vessel was to have been permanently installed in the CFTL and the bundle was to have been inserted or removed from the top of the vessel with the electric hoist located in the penthouse. With the "inverted" design, of course, this is not possible. Since the bundle is now inserted from the bottom of the vessel, each time it is to be removed for inspection or replacement the entire vessel-bundle assembly will be removed from the loop. The hoist will then be used to place the assembly horizontally on a transporter, which will be used to carry it to the bundle assembly work area.

The need to moderate the temperature of the helium exiting the bundle and the desire to x-ray the test bundle during operation led to the development of the attemperation flow concept. Attemperation helium flowing directly from the circulators enters the vessel through nozzles separate from the main bundle flow and is contained in an annular region near the inside wall of the vessel shell by the thermal shroud. This flow cools the shell, maintains the mechanical strength of the material, and permits the use of a thinner shell, which is more easily penetrated for radiography. To further minimize the wall thickness, a high-strength material has been selected for this section. The high-strength, low-alloy steel called for in the preliminary design has been replaced

with Inconel 625 as a result of difficulties in procuring the low-alloy steel in the needed form and expected difficulties in fabricating the transition joint between the low-alloy steel and stainless steel.

The Inconel 625 is radiographically more opaque per unit thickness than stainless steel, but its much superior strength at the design temperature allows the use of a section that is sufficiently thinner to more than offset its increased opacity. Tests are being conducted by the Instrumentation and Controls Division to determine more precisely the radiographic properties of the Inconel 625 section.

At the point the helium flow exits the test bundle, it is extremely hot for certain test conditions. Thus thermal protection is needed for the pressure-retaining boundary in this outlet plenum region. This is provided by using a funnel-shaped flow diverter to prevent the helium from contacting the shell as it passes through the outlet plenum into the outlet pipe and by providing corrugated metal thermal insulation to reduce radiation heat transfer from this diverter to the vessel shell. A static mixer in the outlet pipe promotes the mixing of the bundle and attenuation flows and ensures more moderate temperatures for the piping and heat exchanger downstream.

The analysis required to confirm the design was largely completed in 1979. Since the test vessel is to be an *ASME Boiler and Pressure Vessel Code*, Sect. III, Class 1 component, the design rules of that section were followed. The required stress analysis of the vessel has been completed, including finite-element computer analyses of the Grayloc hub region and the Inconel 625 to type 304 stainless steel transition joint. At certain operating conditions the outlet nozzle region exceeds 427°C (800°F) and thus is subject to the rules of Code Case N-47 for vessels in service at elevated temperatures. A finite-element study of this region has been conducted, and the results have been analyzed according to the procedures of the above-mentioned case. Fatigue analyses for the various sections of the vessel have been performed for the cyclic history described in the design specification. Stresses due to external loads at the nozzles and supports have been calculated and combined with the appropriate pressure and thermal stresses. All stresses are within the allowable limits prescribed by the applicable code.

Additional analysis tasks completed include the derivation of expressions for pressure drop through the test vessel and outlet pipe (excluding the test bundle) and the evaluation of flow-induced vibration effects on the flow diverter and outlet plenum insulation. All pertinent analyses, along with the design drawings, will be compiled and submitted to the CFTL Design Review Team early in 1980. Advanced procurement of vessel shell materials is nearly complete, and fabrication of the vessel is anticipated shortly after completion of the design review.

1.3.1.7 Building Facilities — W. R. Clark

The construction and installation of the structures and services provided to support the CFTL were begun in CY 1979 and by year's end were approximately 95% complete. The design for the site modifications¹⁴ and utility services, which was largely completed by UCC-ND Engineering and detailed in the previous annual report, underwent some relatively minor revisions as a result of CFTL changes and the receipt of more detailed information from equipment manufacturers. Figure 1.8 presents a drawing of the loop and its support systems.

A penthouse has been added to the roof of Building 9201-3 with an opening through the existing roof into the loop enclosure. Because of the increase in test vessel size and the changes in handling procedure (see Sect. 1.3.1.6), the capacity of the electric hoist to be used for handling the test vessel and bundle has been increased to 4500 kg (10,000 lb). A corresponding increase in the penthouse support structure strength was also necessary.

As part of the two cooling air systems, filter houses and structural supports for the ductwork, fans, and drives were constructed. The structural support for the centrifugal fan in the HX-1 cooling air system was revised to accommodate the manufacturer's requirement that 5625 kg (12,400 lb) of concrete be poured into the fan base to improve vibration damping. Unusable equipment has been removed from inside Building 9201-3. This included inactive ventilation systems, lighting, monorails, and miscellaneous structures.

A new steel wall has been erected on the second floor to isolate the high-temperature, high-pressure helium system from the general building,

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Fig. 1.8. Artist's View of CFTL Loop, Control and Data Acquisition Room, Electrical Equipment on the First Floor, and Cooling-Air Equipment on Roof.

protect personnel and operations in the building from the heat and noise generated, and limit access to authorized personnel. A portion of this enclosure is being redesigned to permit easier access to the larger test vessel; construction of this area is awaiting completion of the design. Overpressure protection for the enclosure has been provided by the installation of a blowout hatch in the east exterior wall.

A control room enclosed to provide thermal insulation, fire resistance, and noise attenuation is under construction adjacent to the loop enclosure. The building electrical, instrument air, and sprinkler systems have been modified to provide adequate service, and a small heating, ventilation, and air-conditioning system has been added.

The electrical equipment necessary to provide the test bundle power is located on the first floor. Enclosures to isolate the equipment from the general building, improved lighting to facilitate its maintenance and inspection, and a ventilation system to remove the heat given off from the equipment by forcing outside air through the room are being constructed. This ventilation system includes six new $13.37\text{-m}^3/\text{s}$ (28,320-cfm) fans in the existing filter house at the west end of Building 9201-3, a bank of exhaust fans in the east end of the building, and the installation of new ductwork and modification of the existing air tunnel beneath the first floor.

Several modifications have been made to the building and its utilities to accommodate the CFTL. Structural modifications include new openings in the exterior walls of the loop enclosure area where penetrated by the cooling-air ducts, openings in the second floor for supplying electrical power to the test bundle, and reinforcement of the first floor to support the electrical equipment. Some of the additional utility services being provided are process water to the helium circulators and electrical equipment and instrument air to the actuators in the cooling-air systems on the second floor and the roof of the building.

The site was toured in December with the responsible designers from the various disciplines in UCC-ND Engineering involved in the design. A list of the few items needing completion or correction was transmitted to the construction engineer for action before the job is finished.

1.3.2 Electrical Equipment and Systems — R. B. Biggs

All the equipment for the primary power distribution system was received and installed at the CFTL site. Design of control cabinets for the test bundle power was completed and the purchase of associated equipment was initiated. Purchase orders were placed for the first zone power supply and control system. The design of the auxiliary power system was completed. A purchase order was placed for two variable-frequency power supplies for the helium circulators; a power supply for the third circulator had been purchased previously.

1.3.2.1 Primary Power Distribution System — C. F. Holloway

The primary power system¹⁵ consists of a 13.8-kV circuit breaker; two 2500-kVA, one 1000-kVA, and one 750-kVA transformers; and four units of 480-V switchgear. These items were received from the vendor, installed in Building 9201-3, and connected as shown in Fig. 1.9, but not energized. However, the system had been energized and tested at the vendor's facility to demonstrate that it complied with the specification and QA plan.

Fig. 1.9. One-Line Diagram of CFTL Primary Power System.

1.3.2.2 Test Bundle Power Supply and Control — R. B. Biggs, C. F. Holloway, and T. L. Hudson

The test bundle power supply¹⁵ consists of 13 zones of 500-kVA capacity each, one for each row of FRSs in a 91-rod bundle. An individual zone (Fig. 1.10) is a linear multipower level (LMPL) ac power supply that consists of an induction voltage regulator and a transformer that has 15 selectable taps in the secondary winding to provide 15 equal increments of power. The taps are SCR controlled via a solid-state automatic or manual control system, whereas the induction voltage regulator is automatically or manually positioned with a dc motor as indicated in Fig. 1.11. Other equipment in the circuit from the SCRs to an individual FRS, as shown in Fig. 1.12, includes a protective fuse, automatic "crowbar" protection components, patch panel, power measurement devices, power trim resistor, and test jacks. A block diagram of this equipment, which is mounted in control cabinets, is shown in Fig. 1.13.

The zone power supply is protected in the event of false firing of an SCR or failure of an SCR caused by shorting. The combination of a current-limiting fuse and an adjustable overcurrent detector will be provided for each pair of SCRs in the zone supply to protect against overload and short-circuit faults. The current-limiting fuses will provide protection for large short-circuit currents, and the overcurrent detector will be set to detect overcurrents that may not be sufficiently large to blow the current-limiting fuses. If overcurrent is detected, the zone supply controller will remove the signals from all SCR gates and the main breaker of the supply will be tripped. Other contacts will be available to initiate additional protective action.

The test bundle power and control system is designed to supply all the FRSs in a 91-rod test bundle. The system also is capable of being expanded conveniently to supply as many as 122 FRSs. Test features to facilitate system maintenance and troubleshooting were incorporated into the design, and operational interlocks were provided for system and personnel safety.

The Title II design of the cabinets shown in Fig. 1.13 was completed and procurement of components initiated. In addition, some fabrication was begun; the remaining fabrication will begin in 1980.

Fig. 1.10. Zone Power Supply for CFTL Test Bundle. Shown are induction regulator and transformer having 15 taps selectable programmably through the use of SCRs.

Fig. 1.11. Electrical Drive System for Zone Induction Voltage Regulator in CFTL Test Bundle Power Supply.

Fig. 1.12. One Zone Power Supply Feeding One FRS in the CFTL Test Bundle.

Fig. 1.13. Block Diagram of Zone Power Supply.

Three water-cooled balance resistor cabinets and 13 water-cooled 45-kW dummy loads were purchased and received. These units were energized and tested at the vendor's facility to demonstrate compliance with specifications and QA plan.

Purchase orders were placed for one zone power transformer and one induction voltage regulator with options for the purchase of 12 additional units of each. The units on order are currently in the design and fabrication stage. They will be tested and evaluated before we exercise the options to purchase additional units.

1.3.2.3 Auxiliary Power System — R. B. Biggs

The design for the auxiliary power system, consisting of a motor control center and various other electrical controls necessary for the test loop support systems, was completed. The purchase requisitions are scheduled to be released the first quarter of CY 1980. The cable tray and conduit layout design was also completed, and the drawings will be issued for construction in February 1980.

1.3.2.4 Power Supply for Circulators — T. L. Hudson

On December 19, 1979, UCC-ND Purchasing Division awarded the contract for the purchase of two additional 250-kVA variable-frequency solid state power supplies to Servo-optics Systems, Inc., Dallas, Texas. This is the same manufacturer that built the first power supply¹² that was purchased and loaned to MTI to supply the power for testing the circulator prototype.

1.3.3 Instrumentation and Controls — P. G. Herndon

The CFTL loop dynamics simulation code, ORCULT, was developed further as loop component designs evolved. A report containing a detailed description of the current capabilities and modeling techniques used in ORCULT was completed. Application of the code to several cases to predict the capability of the Stage 1 loop design to accommodate the tests specified is described.

Specifications for the helium flow control valves including the hydraulic actuators were prepared, and purchase orders were placed for the two packed stem-sealed valves needed for Stage 1.

Evaluation tests of a commercial vortex shedding flowmeter (VSFM), modified for CFTL use, were successfully completed. The tests proved that a single meter will measure all CFTL flows. Therefore, the modified VSFM was accepted in place of the venturi as the CFTL reference design flowmeter.

Improvements in the performance of the test bundle power controller were made, and new features were introduced. Two controller prototypes were fabricated and have performed well during field tests.

A written description of the central control system design was prepared and presented to the CFTL Design Review Committee to complete the Title I phase of the CFTL instrumentation and controls design. Specifications were completed and purchase orders placed for the direct digital control (DDC) computer components.

Control, alarm, and prototypic interlock circuits were developed for the zone power supplies, helium circulators, and other loop components.

All the data acquisition system (DAS) equipment except the analog input subsystem was procured in 1979 and is undergoing acceptance testing. The analog input subsystem is scheduled for delivery early in CY 1980.

The locations of the x-ray imaging markers in the FRSS for bundle AGI were specified.

The design of five instrument cabinets to contain test bundle signal conditioning equipment was completed and issued for construction.

1.3.3.1 Dynamic Simulation and Control Analyses — S. J. Ball

The CFTL loop dynamics simulation code, ORCULT,¹⁷ was developed further as control design was developed. The code is a digital computer simulation utilizing the IBM Continuous System Modeling Program (CSMP).

We modified ORCULT to account for the expansion cooling effect that occurs during design-basis depressurization accident (DBDA) transients. A two-node approximation of the helium internal energy equations was used. The nodes corresponded to the two nodes for the conservation of mass

equations used previously. For a reference DBDA case [91 rods, 28 kW/rod, and depressurization via a 0.048-m^2 (75-in.²) hole to a final pressure of 0.16 MPa (23 psia)], addition of the expansion cooling equations caused a maximum change (reduction) in predicted test section inlet temperature of about 4°C. The maximum difference in the predicted inlet temperature to HX-1 during the transient was about 11°C.

A report¹⁸ containing a detailed description of the current capabilities and modeling technique used in ORCULT was completed.

Applications of the ORCULT code included an adaptation to the 61-rod blanket bundle case to predict the capability of the Stage 1 loop design to accommodate the transient tests planned. The main concern was the thermal shocking of the pressure boundary in HX-1 during a simulated GCFR scram test or after inadvertent loss of bundle power. Since there were no expressions available for test section flow resistance and heat transfer for the blanket bundle case, rough estimates were used. The preliminary results indicated that for the worst case (30.8 kW/rod), the maximum rate of change of HX-1 inlet temperature is about 15°C/s, with an overall change of about 80°C in 10 s. These results were evaluated (by others) in terms of allowable stresses, and the pressure boundary transient was judged to be acceptable.

A fine-structure dynamic simulation of a typical FRS was run to determine the cladding temperature responses during fast power and flow transients. Cases were run with both a smoothly varying power input and a power input that switched (optimally) between taps with the 15-level LMPL control. The maximum differences in predicted cladding temperatures for a maximum power case (45 kW/rod) power-flow transient (100 to 40% in 0.7 s) were very small, <0.05°C.

In another study, ORCULT was used to investigate the maximum pressure transients that would occur from various postulated loop overheating transients. This was done to assist in sizing the pressure relief valve, which vents the loop to prevent the pressure from exceeding the design value of 11.8 MPa (1715 psia). The conclusion was that the postulated overheating accidents were unsuited for sizing the valve. The automatic safety system that protects the bundle and loop from the overheating

casualty events shuts down the CFTL before the design pressure level is reached. Therefore, we recommended that the sizing of the pressure relief valve be based on considerations of system makeup flow accidents.

Studies of CFTL dynamic behavior were done with the new upflow design, the larger FRS, and modifications to the loop piping. Several test cases were run for comparison with results of studies done on the previous downflow design. Results indicate that the maximum temperatures seen by the loop components following DBDA tests will be somewhat lower than those predicted for corresponding tests with the downflow design.

Support to the instrumentation and controls design team was also given on loop control and on specifications and requirements for the DDC.

1.3.3.2 Helium Flow Control Valves and Actuators — A. D. McNutt

A specification for the helium flow control valves,¹⁹ including actuators, was prepared in 1978 and sent to vendors for bid in March 1979. Only one bid was received in response to invitations to more than ten vendors, and this bid was too high. On investigation, we determined that two aspects of the specification discouraged vendors from responding. The first, an extremely stringent bellows qualification testing, was corrected by reducing our bellows requirements to what we considered to be the minimum acceptable.

Our second difficulty concerned the valve actuator. While our speed and stroking force requirements for the actuator are not unusually severe for hydraulic systems in general, they are more stringent than are usually encountered in the process control valve industry. With their relatively small engineering staffs, our potential vendors for valve bodies were unwilling to commit themselves to an actuator requirement that did not have an "off the shelf" solution. We therefore decided to separate the actuator and valve body specifications, procuring the valve bodies from vendors who were prepared to fulfill our pressure boundary requirements and procuring the actuators from vendors who specialized in hydraulic components and systems.

A revised specification for bellows-sealed valve bodies, without actuators, was sent out for bid in August 1979. At that time it was

evident that bellows-sealed valves could not be delivered in time to meet the CFTL construction schedule. Packed-stem seal valves, while not completely satisfactory for operation in a helium system, could be procured on a much improved schedule, and, we believe, would provide tentatively for Stage 1 operation of the CFTL. A specification for packed-stem seal valve bodies was prepared and sent out for bid at the same time as the bellows-sealed valve specification.

An order has been placed for the packed seal valves, and we are currently awaiting vendor drawings for approval. A vendor has been tentatively selected for bellows-sealed valves, but no order has been placed because price negotiations are not yet complete.

We prepared a specification for a valve actuation system. It details the requirements for the hydraulic valve actuators, a hydraulic power supply, and the assembly of the actuators to the valve bodies. This specification has been sent to prospective vendors, and we are currently awaiting bids.

1.3.3.3 Helium Flow Measurement — S. P. Baker, R. M. Ennis, Jr.*

A review of all potential helium flowmeters that might fulfill the wide range of CFTL performance requirements concluded²⁰ that a venturi using multiple stacked differential pressure transmitters would be a feasible reference design. Also the commercial VSFM²¹ was thought to be a promising candidate; however, sufficient data were not available to predict its performance for the range and conditions of CFTL helium flows. Since the VSFM promised substantial capital and operating cost advantages over the venturi reference design, an evaluation of a commercial VSFM, modified for CFTL use, was undertaken.

Three issues were to be addressed in the VSFM evaluation, namely:

- verification of the linearity and accuracy of the meter calibration factor over the range and conditions of CFTL helium flows,
- determination of the rangeability of the meter under CFTL conditions,
- establishment of a suitable means of remotely locating the thermal vortex sensor from the relatively high CFTL helium temperatures.

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No suitable helium flow loop was available in the Oak Ridge area to perform the evaluation, and we were unable to find one elsewhere in the United States. An existing compressed air flow loop in the Y-12 Plant area, which uses a concentric orifice meter as a calibration standard, was modified to accept the VSFM. A 100-kW heater and the VSFM were installed downstream of the orifice meter. Flow control valves and pressure and temperature sensors were installed as required, and readouts for the sensors were furnished. Table 1.1 compares the flow conditions of the compressed air loop with those of the CFTL.

Table 1.1. Comparison of Compressed Air Loop and CFTL Test Bundle Flow Conditions

Facility	Fluid	Fluid Density (kg/m ³)	Reynolds Number	Pressure (MPa)	Temperature (°C)
Evaluation loop	Air	1.6-8	25,000-800,000	0.1-0.7	24-260
CFTL test bundle	Helium	0.15-6.8	7,000-900,000	0.2-10	350

In these air flow tests the linearity and accuracy of the modified VSFM were validated over the range of conditions shown in Table 1.1 to within $\pm 2.4\%$, as shown in Fig. 1.14. The orifice meter standard was calibrated by means of a computer program, which provided 11 correction factors and $\pm 0.5\%$ accuracy. However, the combined error from the differential pressure transmitters and pressure and temperature sensors associated with the orifice meter flow measurement was $\pm 2.4\%$.

During the air flow tests the effectiveness of remotely locating the thermal vortex sensor was validated. The high temperature operating limit of the sensor was 70°C. At air flowing temperatures as high as 285°C, the steady-state temperature of the remote sensor mounting block only increased 15°C above room temperature.

Also the low-level signal cutoff of the sensor occurred at 30,000 Reynolds number in these air flow tests. Signal attenuation introduced by remotely locating the sensor was a factor of 10. A number of tests were run to determine the variation of the remote sensor signal with remoteness

Fig. 1.14. Deviation of Vortex Shedding Flowmeter from Orifice Meter Standard in Air Flow Tests.

geometry. The remote signal was found to vary as $D/\sqrt{L}$, where D is the tubing bore diameter and L is the length. In order to project to signal levels in helium flow at CFTL conditions, the variation in signal was also measured as a function of volumetric flow and flow density. The signal was found to vary as ρV^2 . Additional tests were run to determine both the sensor signal and the frequency response of the signal conditioning electronics over the range from 2 to 400 Hz. A factor of 4 variation in signal over this frequency range was found. We determined that a higher gain sensor would be required to provide sufficient signal over the range, as well as a reasonable adjustment of sensor remoteness geometry to reduce attenuation.

A new, higher performance sensor with 2.3 times the sensitivity was obtained, and several iterations to the sensor remoteness geometry were carried out. As a result, a much improved signal-to-frequency response was achieved. A dynamic VSFM range of 154 to 1 in volumetric flow was demonstrated.

The signal cutoff characteristics observed in these air flow tests indicated that sensor signal levels at low flow and low density might be questionable for helium flow at the 0.2-MPa CFTL test conditions. Therefore, several helium flow tests at the 0.2-MPa density were made with the VSFM.

A helium supply was designed, constructed, and installed in the air loop. Using a 1100 std m³ helium truck trailer as a source provided the following helium flow conditions:

Fluid:	Helium gas
Reynolds number:	6000-900,000
Flowing pressure:	0.1-0.45 MPa
Flowing temperature:	20-250°C
Mass flow:	23-67 g/s

Helium flow tests under the above conditions confirmed that VSFM signal performance was acceptable under worst case CFTL helium flowing conditions. However, during these tests an additional iteration of the sensor remoteness geometry was made. The 0.2-MPa sensor signal frequency response for helium is plotted in Fig. 1.15. This is the performance of the modified meter at worst case CFTL conditions.

From the results of both the helium and compressed air flow tests, we conclude that the VSFM, as modified by ORNL, will accurately measure all CFTL helium flows at and above 10,000 Reynolds number scheduled for the 0.2-, 3.0-, and 9.0-MPa tests. Usable signals between 6000 and 10,000 Reynolds number were observed. We projected that the modified VSFM will also accurately measure helium flows in the single CFTL test that extends below 10,000 Reynolds number.

Because this single meter will measure all CFTL flows, it provides substantial performance, capital, and operating cost benefits over the multiple venturi meter approach. Therefore, the modified VSFM was accepted in place of the venturi as the CFTL reference design flowmeter.

A conceptual design of a VSFM for the CFTL as shown in Fig. 1.16 was completed. One VSFM will be installed at each of the four flowmeter positions in the CFTL. Implementation of this design was initiated under a plan that provides for detailed design at ORNL, procurement of selected meter components from the vendor, fabrication of pressure boundary and sensor remote mounting hardware by UCC-ND, calibration at a vendor facility, and installation in the CFTL by UCC-ND.

Fig. 1.15. Sensor Signal Frequency Response for Modified Vortex Shedding Flowmeter at Worst Case CFTL Conditions.

Fig. 1.16. Conceptual Design of Modified Vortex Shedding Flowmeter for the CFTL.

1.3.3.4 Test Bundle Power Controllers — D. W. McDonald

Progress during this reporting period on the bundle power controller has centered around improvements in controller performance and the addition of new features. Two stand-alone controllers have been constructed (Fig. 1.17). One was used with a 15-tap transformer in a laboratory setup for evaluation of performance and design improvements. The second has been installed on a 50-kW five-tap system and is used for the thermal cycling of FRSs. During five months of operation there have been only two failures related to the power controller. The first occurred when impulses as high as 200 V were induced in the power line near the 15-tap system, and the other occurred during testing of a new circuit, which functioned improperly. Overall the controller has demonstrated high reliability.

Fig. 1.17. Zone Power Control Station.

An improved control algorithm was incorporated into the design and overcame several early problems. The controller now demonstrates a steady-state inaccuracy of only 0.5%. Linearity has been improved, and the controller now optimizes cycle sharing for minimum instantaneous error.

Based upon operating experience with the two fabricated controllers and new program requirements, several new features have been added to the design. Operating experience resulted in design improvements to the operator control panel interface circuitry and the inclusion of error detection and indication circuitry. The error circuitry monitors eight critical controller conditions and shuts off the zone power if an improper condition exists. It then indicates via light-emitting diodes the error condition that activated the shutdown.

New features requested by the program include a master manual mode, a hold line, a dc current detector, and an externally activated scram line. The three original control modes, manual, automatic closed-loop, and auxiliary control, have been discussed previously.²² The new mode, master manual, will be used to turn on manually the requested tap voltage in all zones simultaneously. For instance, tap 15 can be turned on in all zones by dialing it on the master control panel in the master manual mode. The hold line was included to best accommodate transients that require a quick power decrease followed by a slow power decay. During such a transient the controller will turn on the SCR pairs to generate the fast portion of the transient. The control computer will activate the hold line to prevent the controller from changing the power output. Then the induction regulator will be used to decrease the load voltage slowly to simulate the slow power decay. The dc detector was required to detect if dc was being generated within the multiple-tapped transformer. Direct currents on the order of 1% of the rated current can seriously damage the transformer. Such currents would be generated if an SCR failed to open. An externally activated scram line was included to give quick response to an unsafe condition detected by safety circuits external to the controller. These conditions include shorted taps and load overcurrent.

The controller was the subject of an Instrumentation and Controls Division design review and was determined adequate for the task. A

technical memorandum²³ was prepared on the early development effort and is available for use. Unanticipated discrepancies between early design specifications and the expected performance of commercially available components are requiring a redesign of some controller circuits. Final design is expected early in CY 1980.

1.3.3.5 Central Control System — A. D. McNutt

A system design description (SDD) of the central control system was prepared and presented to the CFTL Design Review Committee in May 1979. This document, which describes the principal components and systems to be employed to measure and control helium pressure, temperature, flow, and bundle power, is the maturation of previously described work.²⁴ The design was judged to be adequate by the Review Committee, subject to some minor specifically recommended modifications and subject to future review of the complete design. This work completed the Title I phase of the CFTL instrumentation and controls design.

Control panel layout drawings and one-line signal schematics have been prepared for the heat exchanger controls, circulator controls, helium flow controls, and bundle power controls. Specifications have been prepared for the major panel-mounted components and significant portions of the signal-conditioning equipment.

Orders have been placed for the DDC computer, peripherals, software, and process input-output equipment. Many of the process input-output components have been received, but no testing has been done because the computer and several key components of the input-output system have not yet been delivered.

1.3.3.6 Control and Protective Interlock Circuits — P. G. Herndon, A. D. McNutt, A. H. Anderson, and J. W. Cunningham

Design continued on control and safety interlock systems²⁵ to protect the test loop from damage caused by unsafe operating conditions. Fourteen engineering elementary diagrams of control circuits and annunciator circuits, using relay logic notation, have been prepared for protection of the FRS zone power supplies, helium circulators, and other loop components.

The control and alarm concepts shown on these drawings have been coordinated with the electrical power design group and are presently being reviewed and refined internally within the Instrumentation and Controls group preparatory to preliminary issue for comment.

1.3.3.7 Data Acquisition System — K. J. Cross

The DAS equipment,²⁶ procured in 1979, includes the computer, system software, analog input subsystem, and various computer peripherals, including disks, magnetic tape drives, terminals, and electrostatic printer-plotter. All items that have been received are either undergoing or have received acceptance testing. However, delivery of the analog input subsystem has been delayed.

The analog input subsystem procurement involved detailed design specifications of the subsystem controller. The contract was competitively bid and was awarded to Neff Instrument Corporation. The delivery schedule has slipped significantly from initial estimates but should not adversely affect the DAS or CFTL schedules. Preshipment testing at Neff is scheduled for January 1980, and shipment is anticipated shortly thereafter.

1.3.3.8 X-Ray Imaging System — S. P. Baker, J. E. Batey, and W. D. Dodge

Postevaluation work with x-ray imaging techniques²⁷ for measurement of rod bowing and rod axial motion during test bundle operation was continued. The locations of the x-ray imaging markers for bundle AG1 have been determined. We will place 48 platinum markers, 0.5 mm in diameter and 12.5 mm long, at two axial locations in the heated section. Also, 24 platinum markers, 2 mm or larger in diameter and 12 mm long, will be placed in the unheated portion of the bundle. These larger markers will permit much better imaging when the stage 2 real-time x-ray imaging systems are used.

An exact simulation of the 37-rod AG1 bundle has been made. X-ray imaging markers in accordance with the above marker location scheme have been placed in the simulator. Initial radiographs have been made of the simulator. Work using this simulator is now proceeding on optimizing the radiographic techniques for imaging bundle AG1.

1.3.3.9 Test Bundle Temperature Measurements: Interface with Data Acquisition System — P. G. Herndon and C. Brashear

Design drawings for five cabinets to contain signal-conditioning and patch panel equipment for the FRS thermocouples and for associated wiring details have been prepared and issued for construction.

1.4 TEST BUNDLE DESIGN AND DEVELOPMENT — H. C. Young and C. W. Collins

After extensive design studies the bundle criteria were considered sufficiently firm to start formal design in October 1978 of the first test bundle (initially designated as bundle F-1 but later changed to AG1) containing 37 fuel rod simulators (FRSs or rods).

Since that time General Atomic Company (GA) has introduced a number of substantial changes, all of which impacted the bundle design. The rod-to-rod spacing was changed from 11.2 to 9.8 and then to 12.0 mm, and the rod cladding diameter was changed from 7.46 to 8.00 mm. The six hanger rods located in the corners of the hex duct were changed to three hanger rods located symmetrically in the interior of the bundle. Large changes were made in the lengths of the hex duct and of the FRS. The direction of helium coolant through the test bundle was changed from downflow to upflow, and the requirement that the downstream end of the hex duct be free to bow was changed to require precise restraint at two locations along the hex duct.

Despite these changes the bundle design at the end of December 1979 was 70% complete and is expected to be complete in April 1980. A formal design review has been scheduled at the completion of design.

Sensors and sensor locations for bundle AG1 instrumentation were specified. These include the thermocouples inside individual FRSs, heavy-metal markers inside individual thermocouples to act as markers for the x-radiographic observation of rod and bundle bowing during operation, and piezometer holes for the measurement of pressure and differential pressure in the hexagonal duct. Mounting locations on the duct were specified for vibration and acoustic sensors and attachment methods were designed. The bundle thermocouples were ordered.

Development of FRSs for bundle AG1 was impacted by the need to redesign the rod to accommodate an increase in diameter to 8 mm. Certain rod

fabrication procedures caused the nickel center electrode to melt near where it is joined to the heating element. Studies indicate the need to maintain a high density of insulation in this region and also good heat-transfer contact between the heat-producing components and the cladding. Efforts were initiated to do so.

We tried to enhance the long-time reliability of the thermocouples in the FRSs by reducing residual cold work from thermocouple fabrication to 1% and by reducing the grain size of the thermoelements to below the normal for the industry. An appropriate draw and heat treatment schedule was developed to enhance thermocouple reliability.

Two processes for welding the rod cladding to the tubesheet were evaluated, and the gas tungsten-arc (GTA) process using a precision weld head was selected over the electron-beam welding (EBW) process.

Further evaluation of brazing techniques for attaching electrical connectors to the FRSs was necessary because of the change in rod cladding diameter and rod pitch.

The combination of several factors has caused the schedule for the completion of bundle AG1 to slip about eight months from the previous schedule to near May 1982. The factors include the changes to the design of the GCFR fuel rod, the addition of simulating the GCFR core restraints with the bundle, the difficulty with obtaining acceptable rod cladding, and the difficulties with the pace of the development of the FRSs.

1.4.1 Test Bundle Design — J. A. Seneker

Title II design of the first test bundle AG1, a 37-rod fuel bundle, continued during this reporting period. Approximately 70% of bundle design²⁸ is now complete. During the year significant modifications to AG1 occurred, reflecting the major perturbations in the design concepts of the core and the fuel assembly. Three of the more significant changes will be discussed below: (1) the change from six to three grid spacer support rods, (2) change to an upflow core, and (3) the addition of dual lateral restraints. All these changes have required extensive redesign and redrawing of bundle AG1 during the year and all have been incorporated into the reference AG1 design.

Early CFTL test section designs by GA were characterized by a rod array in which the spacing between rods was maintained with a grid spacer system supported from six hanger rods located in the exterior corner positions of the array. This design was similar to that being tested in the AGATHA loop at the Swiss EIR facility. These tests plus other confirmatory tests by GA proved that such a grid design was susceptible to deformation when subjected to in-plane loads. The design was changed to incorporate a different grid web design, the number of hanger rods was reduced from six to three, and their location was changed from exterior corner positions to interior positions one row in toward the center. This design proved to be significantly more rigid and is now the reference design for AG1. The change in location of the hanger rods, however, affected the design of instrument feedthroughs. Part of the bundle instrumentation to be installed in AG1 is 18 thermocouples attached to various grid spacers to directly measure the temperature of the flowing helium coolant. These thermocouples utilize the hollow hanger rods for access to the appropriate grids. With the earlier design of six corner hanger rods, each rod containing three thermocouples penetrated and extended outside the external surface of the bundle tubesheet. These locations were essentially out of the congested area around the FRS terminals and power leads. However, in the three-hanger-rod interior configuration, the rods and thermocouple leads would come through the tubesheet into the middle of the forest of FRS power leads, an unacceptable situation from an assembly and access standpoint. Therefore, the hanger rod and thermocouple design concept was changed so that the rod terminates at the external surface of the tubesheet. The thermocouples leave the rod at the tubesheet internal surface and run radially outward along this surface to a feedthrough tube located away from the power lead region to a more uncluttered area. This instrument feedthrough concept has been incorporated into the reference AG1 bundle design.

The change in GCFR core flow direction from downflow to upflow also caused considerable redesign of CFTL bundle AG1. Not only was the bundle and vessel assembly turned upside down, but because many other core configuration features were modified, significant redesign of FRSs, hex duct,

and other items was also required. As an example, rod diameter was increased from 7.46 to 8.0 mm, rod spacing or pitch was increased from 11.2 to 12.0 mm, rod heated length was increased from 1130 to 1200 mm, overall rod length was increased from 2220 to 2860 mm, and hex duct length was increased from 2309 to 3990 mm. The number of grid spacers and their axial positions were also changed. The upflow changes did not necessitate changing the basic design concepts of bundle AGI but did require extensive rework of many bundle drawings, which had been completed for a downflow configuration. Those drawings were reworked. Redesign of the FRSs is discussed in Sect. 1.4.3 of the report.

Another substantial change in the GCFR core design was the addition of dual lateral restraints at two axial locations to restrict the bowing and deflection of the fuel assemblies during seismic events. Bundle AGI has incorporated similar restraints in its design, thereby requiring significant redesign of some basic bundle concepts and increasing the complexity of bundle assembly. The CFTL restraint system is tied into the thermal shroud, which is at least an order of magnitude stiffer, in the lateral direction, than the hex duct and rod bundle assembly. Any deflection greater than the built-in clearance between hex duct wall and restraint is resisted by the stiffer thermal shroud. The design of bundle AGI and the test vessel is such that the restraints and thermal shroud must be attached to the bundle subassembly and adjusted before the bundle is installed into the test vessel assembly. The clearance and alignment criteria of the restraint system require more precise location of the bundle into the test vessel and more accurate fixturing and fabrication of the bundle subassembly than had previously been necessary. The restraint design can be described as a cylinder completely encircling the hexagonal bundle duct with an inside surface that is formed into a hexagonal shape such that there is a nominal 0.8-mm gap between the thermal shroud (exterior) restraint pads and corresponding (interior) pads on the hex duct. The restraint pads are attached to the thermal shroud through adjustable ties, which can accommodate misalignment of hex duct and thermal shroud during assembly of up to 17 mm. The two restraints are to be adjusted so that they are aligned concentric with each other within

± 0.5 mm and rotationally aligned within $\pm 0.5^\circ$. Since the hex duct is attached rigidly to the tubesheet but the thermal shroud is connected to the tubesheet through a flexible bellows joint, it is necessary to reproduce accurately the bundle-vessel interfaces during assembly and adjustment of the restraints. This will be accomplished through adjustable fixtures that will reestablish as-built dimensions of the test vessel. Figure 1.7 (p. 39) shows the restraint system in place around the bundle hex duct. All restraint system drawings have been incorporated into the reference AGI design.

Design of the separation between the main and attenuator flow passages was completed. The seal for the separation will be a coated-metal C-ring contained in a groove machined in the vessel support ring and compressed by a flange bolted to the support ring with four long bolts that extend into the tubesheet to provide necessary and convenient access during bundle and vessel assembly. Pressure actuation through the open side of the "C" and the soft coating on the seal assist in making a good seal. As an added precaution, a leak test of this seal and other potential leak paths will be conducted during assembly by temporarily plugging the outlet end of the hex duct and pressurizing the main flow cavity to about 0.34 MPa (50 psi).

Design of the attachment of FRS cladding to the tubesheet to complete the pressure boundary seal was finalized. The method of trepanning the external surface of the tubesheet to mate with the cladding is the same as reported last year, and the method of welding will be the gas tungsten-arc (GTA) process, using a specially designed automatic welding head developed by the ORNL Metals and Ceramics Division. More information about this welding technique can be found in Sect. 1.4.5.1.

The method of connecting power leads to the FRS terminals was also finalized. The leads will be brazed to the terminals by utilizing the same welding head and technique developed for the cladding-to-tubesheet welding. More information on these electrical connections can be found in Sect. 1.4.5.2.

Design of the electrical power and instrumentation disconnect panel that is mounted outboard of the external surface of the tubesheet was also completed. All interfaces of the test bundle with the electrical power

supply and instrumentation monitoring equipment are in the form of quick disconnects, which provide convenient hookup of the bundle to all control and instrument functions. The electrical disconnects are push-pull locking connectors, which mate with plugs attached to the end of each power cable. There are two electrical disconnects for each FRS. Most of the bundle instrumentation consists of six thermocouples installed in each FRS. The interface of these thermocouples with data acquisition equipment outside the bundle is a three-prong plug located on the panels containing the electrical disconnects. The plugs provide conveniently installed connections with power supplies and data acquisition equipment.

Other instrumentation, besides the FRS thermocouples, that has been designed into bundle AGI consists of (1) 18 thermocouples installed on three selected grid spacers to measure directly the temperature of flowing helium coolant; (2) 14 pressure taps installed at selected locations on the hex duct to measure pressure losses across various bundle sections; (3) 16 thermocouples attached to the hex duct exterior surface to measure duct wall temperatures; (4) 6 thermocouples attached to the interior surface of the thermal shroud surrounding the hex duct to measure shroud temperatures and predict duct heat loss; (5) 3 acoustic monitors located one at the bundle inlet, one near the start of bundle heating, and the third at the bundle outlet to measure acoustic noise generated in the bundle; and (6) 4 vibration monitors with two installed on the hex duct at each of two locations, one near the start of bundle heating, the other at the bundle outlet to measure any bundle vibrations.

The leads for all these instruments are contained inside feedthrough tubes, which penetrate and are welded to the tubesheet. The pressure seal around each lead is made with Conax-type fittings at the end of the tubes where the leads exit. Terminations for the leads are located on the power and instrumentation disconnect panel. Another feature of bundle AGI that pertains to structurally related measurements is the use of high-density x-ray markers installed in selected FRSs. These markers produce an easily identifiable image on x-ray film, which can be monitored to detect rod movement and deformation. More information on these markers and on the structurally related measurements of bundle AGI can be found in Sects. 1.2.7 and 1.3.3.8. A final piece of instrumentation presently

being designed into AGI is a thermocouple rake to be installed in the hex duct outlet to measure outlet helium gas temperatures. This rake will consist of an array of five thermocouples spaced across the flow area of the duct and will be capable of measuring the transverse temperature profile of the gas stream.

1.4.2 Structurally Related Measurements — S. P. Baker and P. G. Herndon

Structurally related measurements²⁹ to be made in bundle AGI include (1) internal temperatures of FRSs, (2) temperatures of other selected bundle components, (3) bowing movement of individual FRSs and of the FRS-grid-spacer-duct assembly, (4) differential pressures along duct, (5) duct vibrations, and (6) acoustics of flowing helium.

A specification was prepared for the FRS thermocouples and an order placed for a sufficient quantity for the first bundle, AGI. Thermocouple manufacture awaits definition of a suitable draw and heat treatment schedule to produce thermocouples with thermoelements having the minimum residual cold work and the fine grain structure we deem needed for temperature measurement reliability.

Thermocouple locations were specified and attachment methods designed for the thermocouples mounted on the grid spacers to measure gas stream temperatures, on the hex duct, and on the thermal shroud.

Heavy-metal markers will be installed at selected locations inside individual FRSs and on the bundle duct. X-ray imaging of the markers will be made to measure rod and duct bowing and rod elongation during test bundle operation. The location and size of the markers are discussed briefly in Sect. 1.3.3.8.

Differential pressure (ΔP) sensors will be mounted outside the test vessel and connected to pressure taps on the hex duct inside the vessel through small-diameter tubing. ORNL tests show that a 1.75-mm-ID tube will not appreciably degrade a 3-Hz ΔP variation.

Commercially available vibration and acoustic sensors that can withstand the bundle temperatures for all but a few of the most severe tests were identified. Mounting locations were specified and attachment methods designed.

1.4.3 Fuel Rod Simulator Development — R. W. McCulloch

This section delineates the progress made during 1979 toward the development of a reliable, high-temperature, high-heat-flux FRS for use in CFTL test bundles. The cladding of the FRS (Fig. 1.18) is to be of prototypic GCFR fuel rod cladding material and is to have the same dimensions, configuration, and thermal characteristics as the GCFR fuel rod cladding.

The reference FRS is reentrant; that is, both power terminals enter at a single end. A copper tubular terminal joined to a tubular nickel lead is, in turn, joined to the near end of the variable-width heating element made of Kanthal A-1 alloy, which is designed to provide a cosine heat-flux distribution that has a maximum-to-average heat-flux ratio of 1.21. The center terminal is made of a solid copper rod and is joined to a nickel lead, which, in turn, is joined through an adapter to the far end of the heating element to form the electric-current-carrying circuit in the FRS. Cold-pressed BN preforms provide electrical insulation between (1) the cladding and the heating element and its associated tubular lead and terminal (outer annulus) and (2) the heating element and the center lead and terminal (inner annulus). The BN preforms are individually tamped and crushed during installation. They also provide thermal conduction between the cladding and the heating element. Six small-diameter type K thermocouples (TCs) that have 0.5-mm (0.020-in.) Inconel 600 sheaths and ungrounded junctions are installed inside the FRS cladding. The sheaths of the TCs run longitudinally along the inside surface and are located equidistantly around the perimeter of the FRS cross section.

The total length of the FRS is 2860 mm (112.6 in.), including a heated length of 1200 mm (47.24 in.).

The FRS will be designed to operate at power levels up to a 43-kW maximum and will provide a heat flux of 1.7 MW/m^2 ($530,000 \text{ Btu h}^{-1} \text{ ft}^{-2}$) at a maximum cladding temperature of 1000°C (1832°F) and a matching maximum heating-element temperature at 43 kW of 1300°C (2372°F). Higher cladding temperatures are allowed at reduced values of rod power.

The operating conditions place stringent requirements on all materials used in the FRS. The heating element must operate at the

Fig. 1.18. Cross-Sectional Drawing of 8-mm-Diam Fuel Rod Simulator for the CFTL.

maximum temperature without property degradation. The BN must maintain both high thermal conductivity and electrical resistivity at the maximum temperature; in addition, its properties must remain uniform axially and circumferentially to provide accurate simulation of the cosine heat flux. The FRS must also maintain its integrity while enduring thermal transient tests.

The FRS development program staff at ORNL has performed materials, fabrication, inspection, testing, and failure analysis tasks to prepare procedures that are satisfactory for the production of adequate CFTL FRSs.

During 1979 three conductivity cells and seven FRSs were fabricated and operated for more than 1000 h at steady state and 200 h at transient conditions. During a total of more than 10,000 transient cycles only one TC failure was experienced with 24 TCs installed in seven rods. An FRS operated at up to 1400°C cladding temperature for a short time and at 1100°C for tens of hours.

Fabrication improvements allowed higher density and improved uniformity of the inner and outer B₂ preforms. However, problems were experienced with premature failure of several rod prototypes while being tested in the Single-Rod Sodium Loop (SRSL). There was evidence of a low-melting boron-nickel eutectic formed in the central nickel rod causing it to exhibit a low strain resistance. The most likely cause was determined to be low density of the inner BN preform, which resulted from fabrication problems with the Kanthal heating element. The low density resulted in higher than expected temperatures in the nickel rod, producing a boron-nickel alloy at the nickel grain boundaries.

The FRS was redesigned to increase its diameter to 8.0 mm and length to 2860 mm, and one 8.0-mm-diam prototype was fabricated and tested. The Kanthal A-1 heating element material was demonstrated to be weldable and to withstand thermal cycling tests. Variable-width heating elements, fabricated from Kanthal A-1 strips, were prepared for fabrication of the FRSs for bundle AG1.

We learned that small amounts of cold work (3-6%), put into the thermocouples during the cladding swaging portion of FRS fabrication, not

only caused excessive grain growth during subsequent FRS operation, but also created microcracks within the material. These microcracks later grew to form grain boundary voids and caused embrittlement. The nonswaged FRS eliminates cladding swaging and the associated cold work and has produced excellent TC transient operational capabilities.

The use of type S thermocouples was investigated but little progress was made. We developed a BN backfilled (BNBF) TC that had a transient response more than twice as fast as conventional MgO-insulated TCs. Finally, we developed advanced fabrication techniques that resulted in a TC fabrication process that will improve the reliability of the FRS TCs for bundle AG1. The improvement involved achieving small grain sizes of ASTM 9 (17 μm) and ASTM 11 (9 μm) for the Alume1 and Chromel thermo-elements, respectively.

1.4.3.1 Nonswaged Fuel Rod Simulator Fabrication and Testing

Development of a nonswaged FRS design was initiated in 1978 because it offered several distinct cost saving and performance advantages:

1. elimination of the handling and shipping of rods to and from the manufacturer for roughening of the cladding,
2. cost savings by reduction in the number of FRSs having roughening defects,
3. improved final outside diameter and straightness tolerances,
4. improved dimensional location of the heating elements and thermocouple measuring junctions,
5. receipt of FRS cladding that has cold work identical to that of the GCFR fuel rod cladding,
6. reduction of FRS fabrication costs,
7. increased thermocouple lifetime.

This initial development³⁰ has continued throughout this reporting period.

The two principal operating differences between nonswaged and swaged FRSs are that the nonswaged FRS can be expected to operate at a lower maximum power for the same cladding temperature (because of reduced preform density and thermal contact) and the heat flux during a thermal transient will be less uniform.

Three major criteria established to guide the development of nonswaged FRSs are the following:

1. Uniform heat flux profile, as defined by infrared temperature measurement, in a 1-s core transient should be within $\pm 5\%$.
2. A maximum power-temperature condition product must be as close to that of the swaged FRS (43 kW at 1000°C cladding temperature) as possible.
3. The internal TCs must be able to withstand the thermal transients imposed upon them without premature failure.

The first criterion was demonstrated to be achievable during the initial development,³⁰ so most of our efforts for this report period have been applied toward demonstration of the latter two criteria.

Test results on nonswaged prototypes GNR-L-11P and -12P indicated operation at 30 kW at 1000°C cladding temperature, which is about 70% of the maximum power (43 kW at 1000°C cladding temperature attainable with the swaged design). We believed that improvements to increase the maximum power level of the nonswaged rod at 1000°C could be made in three areas:

1. by increasing the density of the outer annular BN preform and/or orientation to improve its thermal conductivity;
2. by replacing the central BN preforms with similar cold-pressed MgO preforms, which, because of their relatively higher thermal expansion, should provide increased radial interface contact among all components;
3. by changing heating element material from Nichrome V to Kanthal A-1 alloy, which has a 100°C higher melting point.

The third criterion was immediately demonstrated with the nonswaged rod design, whereas it was not attainable with the swaged design. However, the reasons for the sudden increase in TC survival rate during thermal cycling tests were not apparent, so that further investigations were pursued.

With the above rationale in mind the fabrication development can now be detailed. Table 1.2 summarizes the test cells and rod prototypes fabricated during 1979 and includes remarks on their test operation.

Test cells GNR-L-19S and -20S were fabricated primarily to evaluate the effects of fabrication techniques and inner preform material on the effective outer annular thermal conductivity.

Table 1.2. Summary of CFTL Fuel Rod Simulator and Conductivity Test Cell Fabrication and Testing

Rod ^a	Heating Element Material	Tubular Lead Materials	Central Rod Materials	Instrumentation ^b	Test ^c Stand	Remarks ^d
GNT-L-19S	Kanthal A-1	Nickel-copper	Type K TC	None		Similar results to those obtained on previous conductivity cells.
GNT-L-20S	Nichrome V	Nickel	Type K TC	None	WTS	Contained an MgO-15W% BN central preform. K was ~10% higher than with previous test cells. MgO-BN performed well electrically at 1350°C.
GNK-L-21P	Kanthal A-1 (constant width)	Nickel-copper	Nickel-copper	Four CSU type K in Inconel TCs with LJ	HTS	Successfully operated 309 h at SS temperature 700-1100°C with 40 h at 1100°C. Successfully completed ~4000 thermal cycles, some very severe, with no TC losses. Kanthal-nickel butt weld successful.
GNK-L-22P	Kanthal A-1 (variable width)	Nickel-copper	Nickel-copper	Four, type K in Inconel TCs with LJ	SRSI	Operated 248 h SS at 30 kW, 650°C cladding temperature; endured 928 transient cycles with power 18-41 kW, cladding temperature 365-650°C. Failed during 0-30 kW, 1000°C cycle by nickel rod melting. All TCs OK.
GNK-L-23P	Kanthal A-1 (variable width)	Nickel-copper	Nickel-copper	Four, type K in Inconel TCs with LJ	HTS	Successfully operated 284 h at SS temperatures 700-1100°C with 32 h at 1100°C. Successfully completed ~5000 thermal cycles similar to those of 21P. All TCs OK.
GNK-L-24P	Kanthal A-1 (variable width)	Nickel-copper	Nickel-copper	Three, type K in Inconel TCs from SDI; one type S TC; all LJ	SRSI	Fabrication defects in rod, type S TC tab pulled off. Tested in SRSI even though it was not fully acceptable. Endured 19 h of SS and transient operation before failure at 38 kW, 750°C cladding temperature. Fabrication defects led to premature failure.

Table 1.2. (Continued)

Rod ¹	Heating Element Material	Tubular Lead Materials	Central Rod Materials	Instrumentation ^b	Test ^c Stand	Remarks ^d
GNK-L-25P	Kanthal A-1 (variable width)	Nickel-copper	Nickel-copper	Four, type K in Inconel IJ TCs; two from CSG; two from SDI	HTS	Operated 100 h, 35 h at 700°C. Ran approach-to-cladding-melting tests 700-1400°C cladding temperature. Rod survived 1 min at 1400°C cladding temperature in vacuum, later reached 1300°C cladding temperature in helium before failure.
GNK-L-26P	Kanthal A-1 (variable width)	Nickel-copper	Nickel-copper	Four, type K in SS, BNBF IJ TCs from GMI	SRSL	Experienced fabrication problems in Ni end plug region but corrected. Rod operated 32 h SS and transient at 10 to 29 kw, 740-780°C cladding temperature. Premature failures occurred by melting of Ni rod adjacent to end plug.
GNK-L-27P ^e	Kanthal A-1 (variable width)	Nickel-copper	Nickel-copper	None	SRSL	Rod operated 70 h SS 10-34 kw at cladding temperature 700-925°C. Failure was similar to that of 26P.
GNK-L-28S	Kanthal A-1 (constant width)	Nickel	Type K TC	None		Test cell was fabricated to further evaluate effects of preform length and tamping energy on FKS transient properties. IR scans indicated preform length should be maintained at 10 mm or less.

¹Serial number postscript S indicates conductivity cell and postscript P indicates FKS prototype.

^bIJ = insulated junction, TC = thermocouple, SDI = Sensor Dynamic Inc., GMI = Grotr Mazur Industries, CSG = Claude S. Gordon, BNBF = BN backfilled.

^cHTS = water test stand, HTS = helium test stand, SRSL = single rod sodium loop.

^dSS = steady state, IR = infrared, K = thermal conductivity.

^eFirst 8-mm-diam fuel rod simulator.

Cell 19S was fabricated similar to 17S and 18S, reported previously,³⁰ but with a Kanthal A-1 heating element. The newly developed Kanthal-to-nickel laser butt weld, joining the heating element to the tubular terminal, was evaluated from both fabrication and operational standpoints. The test cell was successfully fabricated and tested in the Water-Cooled Test Stand (WCTS). The weld joint endured several hundred severe thermal transients without failure or degradation. The decision was made on the basis of this operation to fabricate several Kanthal A-1 elements for full length prototypes.

Test cell GNT-L-20S also contained a Kanthal heating element but was primarily fabricated to test newly developed 85% MgO BN central preforms. The hybrid preforms were intended to provide increased internal thermal expansion and thus increase the thermal conductance at the outer annular preform interfaces. The preforms were developed and fabricated locally and contained BN only to increase their presuability. The postporification strength was less than that previously experienced with BN preforms but was adequate for fabrication.

Figure 1.19 compares effective annular BN thermal conductivity of test cell 20S with that of an earlier test cell, which contained central BN preforms. An increase in thermal conductivity of 10 to 15% is evident for cell 20S, indicating that the MgO-BN preform is effective. However, low-power thermal conductivity is still relatively low, and the maximum values obtained are less than those measured for the swaged construction. Although the use of MgO-based central preforms improves operational conditions, this improvement alone apparently will not increase maximum power for the nonswaged FRS sufficiently to match that of the swaged FRS.

However, in combination with the higher internal temperatures possible with a Kanthal A-1 heating element (50°C higher since the nickel rod becomes the lowest melting internal component) and improved outer annular BN preforms, the nonswaged rod maximum operational condition should approach that of the swaged rod with a Nichrome V heating element.

We thus initiated an informal development task with Eagle-Picher Industries (EPI), the industrial supplier of BN preforms. They had other

Fig. 1.19. Thermal Conductivity for Conductivity Cell GNT-L-14S, Containing Inner Annular BN Preforms, and for Conductivity Cell GNT-L-20S, Containing Inner Annular Preforms Made of 85% MgO-15% BN.

needs for a cold-pressed MgO preform as well as a large amount of expertise in cold pressing technology. They developed techniques to cold press 100% MgO preforms.

Preforms of MgO for the inner annular insulation region in the CFTL FRS were received recently from EPI for our evaluation. The initial density was 75% of theoretical and the preforms had good strength after purification. However, when we attempted to fabricate an FRS with them they were very abrasive and caused severe spalling of copper from the tubular and rod terminals during filling. Additionally, the density decreased from 75 to 62% of theoretical when they were crushed (BN preforms usually gain 2-6% in density when crushed and have an "as-filled" density of 85-90% of theoretical). The "as-filled" powder was also very loose and exhibited very little contact with the surrounding components.

Discussions with the preform manufacturer as well as our own investigations indicated that MgO powder properties such as maximum particle size, particle size distribution, hardness, and particle shape contributed to these poor results. Magnesia crystallites are harder than those of BN and approximately spherical, whereas BN crystallites are flat, thin platelets, which tend to slide over each other.

These discouraging results made it clear that if cold-pressed MgO preforms are to be made usable for the CFTL FRS, then a thorough analysis and optimization of the powder properties for fabricability is needed. A decision was made to retain BN preform for use in the FRSs for bundle AG1 and to continue the investigation into a potentially useful MgO preform.

A series of six rod prototypes, GNK-L-21P through-26P were fabricated and tested. Prototypes 21P, 23P, and 25P were tested in the Helium Test Stand (HTS) while those ending in an even number, 22P, 24P, and 26P, were tested in the SRSI.

Tests in the HTS were intended (1) to determine the reliability of rod internal thermocouples under thermal transient conditions, (2) to determine the capability of an FRS to withstand 1000 to 1200°C operation, and (3) to determine if operation of the FRS was possible at or near cladding melting conditions (1375°C cladding temperature).

All prototypes contained Kanthal A-1 heating elements and all were variable width except that of 21P. Table 1.2 summarizes the pertinent components of each prototype.

Tables 1.3 and 1.4 summarize the steady-state and transient operations, respectively, of the three rod prototypes tested in the HTS.

Rod GNK-L-21P survived 309 h of steady-state tests including over 40 h at 1100°C and successfully completed almost 4000 thermal cycles typical of or more severe than those expected in CFTL operation. Although the thermocouples began to experience errors as the testing progressed, none failed because of thermal element grain growth and thermal expansion induced strains. This was significant and demonstrated that, in the nonswaged rod, thermocouples are far less susceptible to this failure. The Kanthal-to-nickel laser butt weld also successfully survived all operational tests.

Table 1.3. Steady-State Operation of Rod Prototypes in the Helium Test Stand

Rod	Power (kW)	Maximum Cladding Temperature (°C)	Time (h)	Medium
GNK-L-21P	1.9	700	125	
GNK-L-21P	3.1	870	121.5	
GNK-L-21P	4.4	1000	19	
GNK-L-21P	5.5	1100	40.5	
GNK-L-21P	6.8	1200	1	
GNK-L-23P	0.2	350	1	
GNK-L-23P	1.5	700	119	
GNK-L-23P	2.4	870	23	
GNK-L-23P	2.7	930	90	
GNK-L-23P	3.4	1000	19	
GNK-L-23P	4.6	1100	32	
GNK-L-25P	1.6	700	95	Vacuum
GNK-L-25P	2.6	820	0.033	Vacuum
GNK-L-25P	3.0	920	0.02	Vacuum
GNK-L-25P	3.4	995	0.03	Vacuum
GNK-L-25P	3.7	1102	0.03	Vacuum
GNK-L-25P	4.0	1220	0.05	Vacuum
GNK-L-25P	4.2	1310	0.03	Vacuum
GNK-L-25P	4.3	1380	0.03	Vacuum
GNK-L-25P	4.7	1400	0.02	Vacuum
GNK-L-25P	1.6	700	3	Helium
GNK-L-25P	2.2	795	0.025	Helium
GNK-L-25P	3.1	905	0.05	Helium
GNK-L-25P	4.2	1005	0.08	Helium
GNK-L-25P	5.5	1100	0.02	Helium
GNK-L-25P	6.2	1200	0.02	Helium
GNK-L-25P	7.2	1250	0.03	Helium
GNK-L-25P	7.4	1260	0.03	Helium
GNK-L-25P	7.9	1300	0.02	Helium

Table 1.4. Transient Operation of Rod Prototypes in the Helium Test Stand

Rod	Power (kW)	Maximum Cladding Temperature (°C)	Time (h)	Cycles	Transient ΔT (°C)
GNK-L-21P	0-3	750	106.0	3425	330
GNK-L-21P	0-7	870	6.2	371	345
GNK-L-21P	0-14.9	950	2.5	150	450
GNK-L-21P	0-14.9	1300	0.02	1	350
GNK-L-23P	0-1.5	750	2.3	68	325
GNK-L-23P	0-3	750	10.7	640	200
GNK-L-23P	1-2	750	134.0	4020	150
GNK-L-23P	0-1.5	750	0.8	22	330
GNK-L-23P	0-6	870	3.8	230	320
GNK-L-25P	0-1.6	700	5.5	200	350

Rod power was inadvertently increased from near 5.5 to 15 kW for 9 s while operating at 1100°C cladding temperature. Although all thermocouples went off scale, the cladding temperature was estimated to have reached 1350°C. The internal temperature of the nickel rod almost surely reached the melting point during this excursion, although no evidence of damage could be detected by electrical checks. However, the rod failed later while being operated at 1100°C cladding temperature for thermocouple calibration checks. Failure occurred by melting of the cladding at several places along the heated length.

The tests conducted on GNK-L-23P were approximately the same as those conducted on GNK-L-21P. A total of 284 h of steady-state operation was logged, 32 h at 1100°C cladding temperature. Nearly 5000 thermal cycles were completed — with no damage to the FRS and no failure of the thermocouples. The rod failed after 32 h at 1100°C operation. Failure was apparently due to a decreasing value of cladding emissivity caused by breakdown of the tantalum coating. Without the tantalum coating, "thermal polishing" of and consequent high temperatures in the stainless steel cladding can occur within a matter of minutes. Failure analysis of the thermocouples indicated that both Chromel and Alumel thermoelements were ductile, even though grains were large, as were the thermoelements in rod prototype GNK-L-21P, which underwent similar tests.

The primary intent of tests on rod prototype GNK-L-25P was to obtain operational data at temperatures approaching cladding melting. Approximately 100 h of steady-state operation at 700°C was logged to ensure that the rod was reliable before approach-to-cladding-melting (ATCM) tests were run. All TCs were operational at the start of the tests.

Two test series were run. The first (see Table 1.3) involved operation in vacuum from 700 to 1380°C (as measured by a type S TC welded to the cladding). Power was increased to provide temperature increases of about 100°C each. Measured cladding temperature increased incrementally to 1400°C. At this point the rod lost power, apparently because of an open circuit. We subsequently determined that an overtemperature switch, set at 1400°C, had terminated the test. A total operational time of 11 min from 1000 to 1400°C was sustained without damage.

The second test series involved operation from 700 to 1300°C in low-pressure [~ 70 kPa (10 psi)] helium (Table 1.3). A total operational time of 11 min at temperatures from 1000 to 1300°C was sustained before failure occurred at 7.9 kW and 1300°C cladding surface temperature. Because of the helium environment, the power at higher temperature was twice that in the previous vacuum test.

Failure in rod 252 was caused by the melting of the nickel center rod. The increased heat flux raised the rod internal temperature sufficiently compared with the vacuum test to cause the nickel to melt ($T_m \approx 1425^\circ\text{C}$) at a cladding temperature of 1300°C. These tests indicate stable operation to cladding temperatures approaching 1300°C at power levels larger than anticipated for GCFR depressurization tests.

High-temperature and transient tests on these three prototypes verified that operation in the 1000 to 1200°C regime is possible and that very limited operation is possible up to and including cladding melting. However, additional 8-mm-diam prototypes will be operated because of the scatter of the test data at high temperatures.

No rod internal thermocouples were lost during testing, although all three prototypes encountered conditions more severe than anticipated in the CFTL. Failure analysis of TCs in the prototypes indicated that, unlike the TCs in swaged prototypes that encountered similar testing, the thermoelements were very ductile. This is despite the fact that grains were very large and often traversed the entire cross section.

This serendipitous, but unexplained, phenomenon was discussed with Brian Ralph from Cambridge University, England, during a visit by him to ORNL. He is a metallurgist who has spent considerable time examining material grain boundaries and materials properties associated with them. Consulting with Ralph during his brief visit led us to suspect strongly that void growth during FRS testing, caused by levels of 5 to 10% of cold work put into the thermocouples during FRS fabrication, is responsible for the brittle behavior of TCs in previous swaged rods such as 7P.

Apparently when the TCs are strained (by FRS swaging, pulling to straighten, or other causes) microcracks appear around material inclusions. These sites result in nucleation and growth of voids later during

high-temperature operation, which causes material recrystallization. The voids form at grain boundaries, reduce the grain boundary cross section, and drastically weaken the material. Although lower levels of prestrain (<1%) associated with nonswaged rods probably result in a minor degree of microcracking, they apparently do not add enough energy to the system to induce void nucleation and growth.

Although subsequent analysis will be performed as funds permit to determine the validity of this theory, previous evidence strongly supports it. Thus, it would appear that the major necessary ingredient for successful long-life high-temperature operation of 0.5-mm type K thermocouples is simply to keep the prestrain levels to less than 1 or 2%, as is done routinely with the nonswaged rod design.

Results from testing rod prototypes 22P, 24P, and 26P in the SRSL were not as encouraging as those obtained with HTS operation of 21P, 23P, and 25P. Tables 1.5 and 1.6 summarize the steady-state and transient test operations, respectively.

Rod GNK-L-22P endured 928 transient cycles and operated a total of 247.5 h at steady state before failure. Most transient cycles were from 350 to 600°C sodium temperature (365 to 650°C cladding temperature) and

Table 1.5. Steady-State Operation of Rod Prototypes in the Single-Rod Sodium Loop

Rod	Power (kW)	Maximum Cladding Temperature (°C)	Time (h)
GNK-L-22P	20	650	25
GNK-L-22P	30	650	221.4
GNK-L-22P	30	1000	1.0
GNK-L-24P	8.5	500	3.0
GNK-L-24P	8	500	1.0
GNK-L-24P	20	650	0.5
GNK-L-24P	30	750	0.5
GNK-L-24P	38.5	710	8.8
GNK-L-26P	10.6	750	2.0
GNK-L-26P	19.1	750	10.5
GNK-L-26P	28.7	780	21.0

Table 1.6. Transient Operation of Rod Prototypes
in the Single-Rod Sodium Loop

Rod	Power (kW)	Maximum Cladding Temperature (°C)	Time (h)	Cycles	Transient ΔT (°C)
GNK-L-22P	19-41	600	23.1	925	250
GNK-L-22P	0-30	1000	0.05	3	300
GNK-L-24P	0-30	750	5.5	495	300
GNK-L-26P	0-19.1	750	3.3	290	250

of 1 to 2 min period with step changes in power between 18.8 and 41 kW. Most of the steady-state operation was at 30 kW and 650°C maximum cladding temperature.

The rod failed when being cycled from 0 to 30 kW at 1000°C cladding temperature. Failure was due to a crack in the nickel center conductor at about 25 mm (1 in.) from the ground end of the heating element. Apparently the combination of rapid rise in power and cladding temperature weakened the nickel rod to the point of failure. The four type K, insulated-junction TCs endured all the thermal transient operations without failure.

Fabrication problems had occurred at the ground end of the heating element with 22P. The variable-width Kanthal A-1 heating element is very strong at room temperature and difficult to work. The fabrication process required that the nickel end plug, inserted before the inner BN preforms were installed, should be removed after preform installation to permit electrical checks. The removal and reinsertion of the end plug proved to be difficult, disturbing the adjacent BN preforms and causing loss of density and of good heat transfer contact.

Although the fabrication procedure was modified, essentially the same conditions were experienced during fabrication of rod prototype GNK-L-24P. The infrared scans of 24P indicated that the density and contact loss was even more severe than that on 22P, but we decided to test the prototype since useful information could still be obtained.

The rod endured about 10 and 9 h, respectively, of transient and steady-state operation at a maximum cladding temperature of about 750°C and power levels to 38 kW before failure. Postfailure examinations revealed that failure occurred at the point of minimum BN density, as indicated on the infrared scan. The reduced density (and thus thermal conductivity) resulted in about 25% reduction in expected rod capability.

The end plug fabrication procedure was changed for GKK-L-26P so that the nickel end plug could be removed with the heating element still encased. Although this modification resulted in an absolute minimum of disturbance to the inner BN preforms during fabrication, we learned that even spot-welding the heating element to the nickel end plug partially loosened the preform adjacent to it, again resulting in lower than anticipated local BN density. It may be noted that these problems also occurred on rod prototypes tested in the HTS, but the lower power levels of operation and a relatively constant axial temperature made the problems much less severe.

Although the low-density region was less severe than with 24P, we decided to test 26P in the SRSL. Operation was normal throughout the tests, which were conducted for about 6 to 7 h each day. On the beginning of the ninth day, we discovered that the current-carrying circuit was open. The rod was removed, inspected, and found to have an opening in the central nickel rod about 10 mm from the end plug. The nickel rod was pulled apart by thermal stresses, and it had operated very close to its melting point similarly to rod prototypes 22P and 24P. There was also evidence of a low-melting (1080°C) boron-nickel eutectic formed at the grain boundaries of the nickel rod, and the grains within the rod were very large, with some traversing the entire 2.16-mm (0.85-in.) cross section.

The boron-nickel eutectic was located primarily at the nickel grain boundaries, indicating that the eutectic was formed at a temperature below the about 1425°C melting point of the nickel. However, rod prototypes 11P and 12P had been tested previously at more severe conditions and the eutectic had not formed. Therefore it can only be assumed at this point that the low inner BN density is the primary cause. This is also supported

by the occurrence of the failures adjacent to the end plug, which should have been at a lower temperature than the "hot spot" region located about 0.25 m up into the heating element.

Rod prototype GKK-L-27P was the first 8-mm-diam prototype to be fabricated. It contained no thermocouples and was fabricated with preforms pressed locally. Several fabrication problems were encountered, primarily with tooling, as is normal when a major dimensional redesign of a rod occurs. The same problems with low density of the inner SN preforms were again encountered in the nickel end plug region, largely because of the increased rigidity of the Kanthal A-1 variable-width heating element. The heating element diameter had been increased from 4.24 to 4.75 mm and the nickel rod diameter increased from 2.16 to 2.59 mm as part of the design change. These changes resulted in less internal terminal heating and increased strength of the central nickel rod and the heating element.

The rod was tested in the SRSI, and the operation is summarized in Table 1.7. The rod failed at 35.3 kW power with the cladding temperature near 950°C. Failure analysis showed that the ground end of the nickel rod had been close to the melting point, and there were several cracks along the nickel rod adjacent to the heated section. This analysis closely paralleled that for rod 26P, except that the nickel rod of 26P exhibited failure near the ground end only.

Table 1.7. Summary of Operational Testing of Rod Prototype GKK-L-27P

Rod Power (kW)	Sodium Outlet Temperature (°C)	Time at Conditions (h)
10.0	704	6.8
19.0	704	21.5
19.0	777	20.5
20.0	927	7.3
26.6	500	0.5
26.0	927	3.0
30.0	927	10.0
35.3	927	0.1

We are modifying the fabrication procedure further to correct the localized low density region. A fixture has been devised to eliminate virtually all movement of the heating element near the end plug area during inspection and welding.

The presence of a boron-nickel eutectic under any conditions is cause for concern. Boron has very little solubility in nickel, so that a eutectic phase can occur above 1080°C at levels of 280 ppm B in nickel. This boron must form from the BN to be available. No reliable data have been found on dissociation of BN to boron, but probably very little BN will dissociate at our operating temperatures. Both the dissociation and the eutectic phase formation depend on time. Although this may limit the useful life of the FRS at high temperatures, tests in the HTS at similar high temperature conditions of the nickel rod did not show the formation of the boron-nickel eutectic to be a problem. Because of the possible time-temperature limitation of a boron-nickel eutectic formation and because of the probable increase in operating capability, the use of MgO central preforms as a substitute is being pursued.

1.4.3.2 Thermocouples for Use in CFTL Fuel Rod Simulators — R. W. McCulloch and J. H. Clift*

Our work during CY 1979 with the small-diameter TCs for the CFTL FRS involved the procurement of them and was concerned with defining and refining our notions³¹ of the previous year for enhancing the reliable life of the TCs. These notions included minimizing the residual cold work in and minimizing the grain size of the Chromel and Alumel thermoelements. In addition, the need for minimizing the presence in the finished TC of oxygen, moisture, hydroxides (such as magnesium hydroxide), and oxides (such as iron oxide) was identified.

Groth-Mazur Industries (GMI) of Addison, Illinois, was chosen as the manufacturer of the 0.5-mm-diam type K MgO-insulated, Inconel 600-sheathed, insulated junction thermocouples.

We provided guidance and evaluation to GMI in the tasks of achieving the minimum residual cold work and in minimizing grain size. Meeting the

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requirements for minimum cold work and for minimizing the presence of impurities in the finished TCs was relatively convenient as is related below. However, achieving the required small grain size for the thermoelements was more difficult and represented the bulk of our CY 1979 work and also is the larger portion of this report.

The requirement for eliminating cold work from the as-received TCs is adequately covered in the specification, which requires a 48-h "ordering" bakeout. This low-temperature heat treatment is conducted after the last anneal and will remove any cold work not previously removed. The fabrication procedure for the nonswaged CFTL FRS ensures that the TCs will receive a minimum of cold work during subsequent FRS fabrication, certainly less than 1%.

The purity requirement was not as easily accommodated. Discussions with GMI revealed that it intended to use its normal bakeout procedure (at ~0.1 mPa pressure and 200°C) on the insulators for this group of thermocouples and further that it intended to anneal the bulk TC material (BML) after each reduction by drawing (~10% in most cases). Since there are known hydroxides that do not break up until well above this 200°C temperature (specifically as high as 900°C), it was apparent that such hydroxides would subsequently be available for oxidation of both chromium and aluminum.^{32,33} As a consequence, we obtained samples of insulators from GMI in both the "as received" and "as processed" conditions. These samples when subsequently baked out in a vacuum furnace at Oak Ridge revealed a lower level of outgassing at low temperatures (<400°C) for the "as processed" insulators but also revealed subsequent release of CO₂, H₂, and other gases at temperatures up to about 950°C.

On the basis of these results GMI agreed to establish a high-temperature vacuum bakeout system and to load the MgO insulators into the BML sheath inside a glove box under argon cover gas. The contract will be renegotiated to cover these two additional efforts.

The TC specification requires ASTM 7 grain size (32 μ m or 0.00125 in. average diameter) for both thermoelements. Historically this requirement has been difficult to attain, and only one of the TC manufacturers has had reasonably uniform success in achieving it. Initial samples from GMI were evaluated by ORNL, and it appeared likely that GMI would not meet the grain

size requirement using its fabrication process. Hence, GMI agreed to provide additional samples in return for sample evaluation and guidance by ORNL.

Initially we studied the details of the dependence of grain size on the amount of draw (cold work), the recrystallization temperature, and the annealing temperature and time at temperature for drawing a solid metallic wire. We were able to reduce thermoelement grain size to one-third that from the initial GMI process. Attention was then focused on the drawing process itself to further reduce grain size. It became apparent that grain size for a solid wire is minimized by increasing the amount of draw between anneal heat treatments to the maximum short of breaking the wire. However, we found that this notion cannot be applied directly to drawing a thermocouple whose structure, consisting of a sheath containing MgO insulation and two thermoelement wires, differs considerably from the structure of a solid wire. Evaluation of other samples of TC material drawn to ORNL recommendations indicated that the amount of draw between heat treatment anneals would best be made in several steps to protect the thermoelement wires from breaking during the draw process. After additional attempts an appropriate draw-anneal schedule was produced to achieve the required grain size for the thermoelements.

The final grain size of the 0.075-mm (0.003-in.) thermoelements is a function of the starting grain size and of a combination of the amount of cold work put into the elements between anneals and the annealing conditions. Figure 1.20 illustrates the typical dependence of recrystallized grain size (RGS) on the amount of cold work given to a typical solid metallic material. As the cold work is increased, more dislocation defects are introduced into the material. When the material, after being cold worked, is heated above a temperature of $T_R \geq 0.5T_{\text{melting}}$, then recrystallization takes place. Recrystallization is a process whereby new grains are nucleated at defect sites and grow into strain-free grains. The transformation has a sigmoidal distribution. Its completion time is a function of temperature, and the number of nucleation sites is given by Shewmon³⁴ as

$$t = \frac{2}{G} \left(\frac{0.17}{n} \right)^{1/3} = \frac{2}{B \exp(-Q_G/RT)} \left(\frac{0.17}{n} \right)^{1/3}, \quad (1)$$

where G is the velocity of transformation from strained to strain-free grains, B is a constant, Q_G is the activation energy, and n is the number of defect sites where nucleation begins.

Fig. 1.20. Recrystallized Grain Size vs Cold Work for α -Brass as Function of Initial Deformation.

The grain size after recrystallization depends on the initial number of nuclei, N , formed in the untransformed material per unit volume per unit time and the velocity of transformation, G , and is given by Shewmon³⁵ as

$$d \approx \left(\frac{G}{N} \right)^{1/4} = \left[\frac{B \exp(-Q_G/RT)}{N} \right]^{1/4} \quad (2)$$

The grain size can then be controlled by controlling N and the temperature. Since N depends primarily on the number of defect sites and thus the cold work, the grain size is reduced as N is increased, as predicted in Fig. 1.20. Thus the smallest grain size will be obtained with the maximum cold work and the lowest annealing temperature above the recrystallization temperature. Equation (1) shows that as T is decreased the time to recrystallize increases. From a practical standpoint this

time must be reduced as much as possible. Annealing is a costly process. It is conducted mostly by pulling long lengths of bulk TC material through a furnace hot zone at a rate determined by the length of the zone and the anneal time.

Previously GMI had established empirically its own anneal process. The time in the hot zone was about 5 min and the annealing temperature was about 1025°C or more than 350°C above the recrystallization temperature of the material.

Analysis of the grain size of TC material made under the GMI conditions revealed that the material was fully recrystallized and well into the grain growth stage. Our initial plan was to put a predetermined amount of cold work into samples of starting material, vary the annealing conditions, and thus determine the best process conditions for producing the smallest grain size of fully recrystallized material. The process was complicated because the three principal components of the thermocouple (Chromel, Alumel, and Inconel 600 sheath) had different minimum recrystallization temperatures, which varied with cold work. Little was known about this dependence, and, in addition, the mass of the TC material being annealed changed greatly with the diameter. The initial diameter of the material to be annealed was 2.84 mm (0.112 in.), and the final diameter was 0.5 mm (0.020 in.), so the mass per unit length changed by a factor of 31.4.

The optimum annealing conditions were determined to be 940°C for 3 min for a TC diameter of 1.25 mm or greater, 940°C for 2 min at a TC diameter between 0.81 and 1.12 mm, and 940°C for 1.5 min below 0.81 mm. Figure 1.21 compares grain sizes of a 1.9-mm-diam TC cold worked 80.2% and annealed at 1000°C for 6 min and the same material annealed at 940°C for 3 min. The grain size was decreased by a factor of 3.

The drawing process was also examined in detail. Figure 1.22 shows the geometry and forces on material being drawn through a die. The three forces acting on the material are axial tensile forces, transverse shear forces, and friction forces. The equations describing the process for a homogeneous wire of constant volume are given by Dieter³⁶ as

Fig. 1.21. Recrystallized Grain Size vs Annealing Time and Temperature for 1.9-mm Al-1.9-mm Thermocouple Material with 80% Cold Work. (a) Annealed at 1000°C for 6 min, average grain size 0.06 mm. (b) Annealed at 940°C for 3 min, average grain size 0.02 mm.

- F_d = DRAW FORCE
- S = SURFACE AREA
- σ_{xs} = DRAW STRESS
- p = MEAN NORMAL PRESSURE ON AREA S
- α = DRAW DIE ANGLE
- μ = FRICTION COEFFICIENT
- L = DRAW DIE ACTIVE LENGTH

Fig. 1.22. Forces Acting on a Wire Being Drawn Through a Die.

$$\sigma_{xx} = \overbrace{\bar{\sigma}_0 \ln(A_b/A_a)}^{\text{Work factor}} \underbrace{(1 + \mu \cot \alpha)}_{\text{Friction factor}} \underbrace{\phi}_{\text{Shear factor}} \quad (3)$$

$$\phi = C_1 + C_2 \left(\frac{D_b + D_a}{D_b - D_a} \right) \sin \alpha, \quad (4)$$

where σ_{xx} is the draw stress on the wire exiting the die, $\bar{\sigma}_0$ is the mean material yield stress, A_b and A_a are the initial and final cross-sectional areas, μ is the coefficient of friction for the lubricated die-wire surfaces, α is the draw die half angle, ϕ is the redundant or shear work factor, and C_1 and C_2 are constants associated with strain hardening. The first factor in Eq. (3) is the work factor, and the last two factors, the friction and shear factors, do not contribute to the wire diameter reduction. The process is expressed more simply, also by Dieter,³⁶ by

$$\sigma_{xx} = \frac{1}{\eta} \int \sigma \, d\epsilon,$$

where η , the efficiency factor, is the ratio of the work term to σ_{xx} , and $\int \sigma \, d\epsilon$ is equivalent to $\bar{\sigma}_0 \ln(A_b/A_a)$, where the variation in stress with strain hardening is taken into account. A material that exhibits strain hardening when plastically deformed can be described by

$$\sigma = \kappa \epsilon^m,$$

where m is the strain hardening exponent and κ is the strength coefficient. Figure 1.23 shows the flow curve for a wire as well as $\int \sigma \, d\epsilon$ for an ideal (100% efficient) process and $(1/\eta) \int \sigma \, d\epsilon$ for the actual drawing process.

The draw stress, σ_{xx} , for a die-produced elongation, ϵ , and the corresponding material flow stress, σ_ϵ , are indicated. The weak point in the system is where the wire exits from the die. The flow stress of the material follows the indicated curve and the material is strain hardened as it is deformed. The drawing limit is attained when the total elongation $\epsilon_{\max}$ is reached, at which point the material flow stress $\sigma_{\epsilon_{\max}}$ is equal to the process draw stress σ_{xx} . When $\epsilon_{\max}$ is reached, whether in

Fig. 1.23. Comparison of "Real" and "No Loss" Drawing Processes with a Material Flow Curve. (After Dieter, *Mechanical Metallurgy*, McGraw-Hill, 1976).

one or several draws, the material breaks. Thus $\epsilon_{\max}$ can be increased only by increasing the process efficiency so that the process flow curve intersects the material flow curve at a larger $\epsilon_{\max}$.

It may be seen that drawing efficiency can be maximized by increasing the $\ln(A_b/A_a)$ in Eq. (3) and by decreasing D_a in the quantity $(D_b + D_a)/(D_b - D_a)$ in Eq. (4). (α is set and not normally changed.)

However, in our work, matters were somewhat different, especially for drawing small-diameter TCs. The reasons for this become more evident when a thermoelement undergoing drawing is examined, as in Fig. 1.24.

The thermoelement is reduced by the frictional forces transferred through the MgO particles. The forces are generated by the draw die acting on the thermocouple sheath. As the sheath diameter is reduced and the insulation gap is decreased, the MgO particles move forward and inward to elongate the thermoelement and thus reduce its cross-sectional area.

Since the surface-to-volume ratio for the TC components is proportional to $1/d$, surface effects become much more important as the TC size is reduced. Figure 1.25 illustrates the importance of surface effects

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Fig. 1.24. Forces on Thermoelement During Drawing.

Fig. 1.25. Chromel Thermoelement in a 0.53-mm Thermocouple. Note local reduction in wire diameter caused by intrusion of an MgO particle.

in the process. A localized diameter reduction of 25% corresponding to a cross-sectional area reduction of about 40% is evident near the right-hand upper corner of the wire. Thus the local material flow stress is reduced proportionally indicating that failure is likely at sharply reduced drawing strain $\epsilon_{\max}$ (Fig. 1.23). The localized thermoelement cross-sectional area reduction was apparently caused by the intrusion of a larger than average MgO particle into the thermoelement surface. As the TC is reduced, MgO particle size becomes much more of a factor since fewer particles span the annulus between the sheath and thermoelements. The mean particle size appears to be about 12 μm (0.0005 in.) with some particles, such as the one in Fig. 1.25, as large as 60 μm (0.0025 in.). When such a particle is present, the localized stresses may become very high. Thus the probability of a local stress failure increases as $1/d$ increases. When the amount of reduction per pass is reduced, the localized stresses are reduced accordingly.

These effects began to be important as the diameter of the drawn TC approached 2 mm. We then had to maintain the area reduction to about 10% per pass to maintain TC integrity.

The maximum cold work (true elongation) of a 0.5-mm-diam TC in a pull test is in the range from 22 to 26%. An elongation of approximately twice that, or about 50%, can be expected from drawing. A production draw-anneal process for TCs must have as few thermoelement breaks as possible, in order to yield useful lengths of thermocouples. Our investigation indicated that the maximum cold work between anneals for TCs of 1.25 mm and less must then be limited to 30 to 35% to maintain an acceptable process. We used this criterion along with that of about 10% maximum area reduction per step and the annealing criterion previously developed to guide the development of an improved cold work schedule. Table 1.8 summarizes the draw-anneal schedule that will be used to produce thermocouples for the FRSs for CFTL bundle AG1. The Al₉Mel thermoelement has the lowest recrystallization temperature and thus is the most sensitive to grain growth during annealing. Therefore, we put an additional 49% cold work into it before the bulk material length (BML) was assembled. The BML was then drawn from 4.76 to 2.84 mm in one pass. This crushed the MgO preform and produced some cold work of both thermoelements, but since the volume was not conserved the amount of cold work could not be accurately determined.

Table 1.8. Thermocouple Draw-Anneal Schedule
for CFTL Bundle AG1

Draw Diameter (mm)	Number of Steps	Cold Work, %		Annealing Time ^a (min)
		Maximum Step	Total	
2.84	1 ^b	^c		3
1.91	4	22.7	80.2	3
1.47	5	11.6	51.4	3
1.27	3	13.3	29.7	3
1.12	6	4.4	25.6	2
0.97	4	9.9	29.3	2
0.81	5	10.2	34.3	2
0.69	4	12.5	33.9	1.5
0.58	4	8.2	32.1	1.5
0.51	3	9.3	28.0	1.5

^aAt 940°C.

^b1 draw to reduce from 4.76 to 2.84 mm.

^cNot determined since the insulation volume was not conserved in this step.

The cold work was maintained above 50% on the first two draw-anneal cycles, although four or five draw steps were performed in each anneal cycle. The cold work was then reduced to about 30% for the remaining passes. Annealing time was reduced from 3 to 1.5 min as the BML diameter was decreased.

The final grain size of the Chromel and Alumel thermoelements was 0.01 mm (0.0004 in.) and 0.02 mm (0.0008 in.) corresponding, respectively, to ASTM 11 and 9. Figure 1.26 shows transverse micrographs of the thermoelement grain structure before and after this effort to reduce thermoelement grain size.

The process as presented in Table 1.8 has been accepted by GMI and will be used to manufacture the thermocouples for the PRSs in CFTL bundle AG1.

ORNL PHOTO 2611-80

(a)

(b)

50 μm

Fig. 1.26. Comparison of Grain Size of GMI-Produced 0.5-mm-Diam Thermocouples. (a) Before revising draw-anneal schedule. Chromel (left) grain size 23 μm (ASTM 8) and Alumel 47 μm (ASTM 6). (b) After revising schedule. Chromel 9 μm (ASTM 11) and Alumel 17 μm (ASTM 9).

1.4.4 Research on Reliability of Small-Diameter Thermocouples for Fuel Rod Simulators - R. L. Anderson

When actual use of the small-diameter thermocouples (TCs) in prototypic FRSs was started, the initial experience was unsettling, since during thermal cycling the TCs had a failure rate of almost 100%. Typical behavior observed in the failures was an open circuit on heating; but on cooling, electrical continuity was reestablished. An intensive effort³⁷ begun last year was continued to determine the cause(s) of the failures and possible remedies. Tabular data, which showed the results of breakage and thermal cycling tests made with small-diameter TCs and were reported previously, are presented graphically in Figs. 1.27 and 1.28, respectively, to reiterate a principal finding: breakage and thermal cycling failures of small-diameter TCs are strong functions of the fabrication process.

Two major effects contributed to the failure of these TCs: differential thermal expansion between the TC wires and the type 316 stainless steel cladding of the FRSs and the growth of excessively large grains, frequently extending across the diameter of the wires. Such a grain structure weakens the wires and results in early failure.

The thermal cycling tests described previously³⁷ were extended to include samples of small-diameter platinum-rhodium-sheathed type S TCs. The results were similar to those obtained with the type K materials; the ability of the type S materials to endure thermal cycling was principally determined by the source from which the materials were obtained. One notable difference in the behavior of the type S materials was found. The materials obtained from Control Products, Inc., initially had a small number of cycles to failure (high failure rate). (See Fig. 1.29 samples marked as-received.) After the materials were stress relieved by heating to 450°C for 2 h, none of the samples from this batch of material failed, even though the number of cycles reached 3600 in one case. In contrast, samples of small-diameter materials obtained from SEMCO, Inc., had a high rate of failure even after heat treatments at both 450 and 800°C.

An exploratory experiment in the fabrication of an Alumei alloy that would resist excessive grain growth was conducted by R. K. Williams,

Fig. 1.27. Results of Breakage Tests with Small-Diameter Type K Thermocouples Sheathed in Alloy 600 or Stainless Steel and Small-Diameter Type S Thermocouples Sheathed in Stainless Steel.

Fig. 1.28. Results of Thermal Cycling Tests with Small-Diameter Type K Thermocouples.

Fig. 1.29. Results of Thermal Cycling Tests with Small-Diameter Type S Thermocouples.

D. L. McElroy, and R. L. Heestand of the ORNL Metals and Ceramics Division. They proposed to fabricate Alumel wire using a powder metallurgy technique successfully used previously to make grain growth resistant Pt-8% W, Pt-5% Mo, and Pt-8% Ru.

Pieces of nominally 1-mm-diam Alumel wire were loaded into a nickel tube and about 1 vol % of 1-mm-diam alumina particles was added. The filled tube was then baked and welded shut under vacuum, and the resulting capsule was hot-swaged. The nickel cladding was then removed and the compacted Alumel wires were drawn down to the original single wire diameter (with intermediate anneals).

Samples of the resulting wire were then heat-treated in a vacuum furnace at 1200°C for 24 h, along with a piece of the original Alumel wire. After this heat treatment, the samples were mounted and polished and etched to reveal the grain structure. Figure 1.30 shows that this technique has succeeded in pinning the fine grain structure in the fabricated wire and prevented the growth of large grains such as those exhibited by the control sample.

Fig. 1.30. Longitudinal Sections of Thermocouple Wires Vacuum Annealed at 1200°C for 24 h to Promote Grain Growth. (a) Alumel control sample from Y-12 stores. Etchant 80 HNO₃-20 acetic acid. (b) Alumel-1 vol % Al₂O₃ produced at ORNL, vacuum annealed at 900°C for 4 h. Etchant: aqua regia.

1.4.5 Development in Support of Test Bundle Design — H. C. Young

The weld process for joining the FRS cladding to the tubesheet was selected during this year.

Development of the method for brazing a copper connector to the concentric electrical terminal on each FRS, tests to measure heat generation in the electrical connectors, tests of methods for removing the heat, and refinement of methods of insulating the electrical terminals and rod thermocouples were continued during CY 1979.

1.4.5.1 Joining Fuel Rod Simulator Cladding to the Tubesheet — H. C. Young and T. B. Conley

At the beginning of the year, two methods were being considered to join FRS cladding to the tubesheet. The two methods were gas tungsten-arc (GTA) welding and electron beam (EB) welding. Other methods had been considered and rejected during previous work.^{38,39} The GTA method for the CFTL application was developed by the ORNL Metals and Ceramics Division and the EB method was evaluated with the assistance of the Electron Beam Welding Company (EBW) of Los Angeles, a firm that successfully welds similar joints in LMFBR-THORS bundles.

In CY 1978, six test capsules, each of which had seven dummy FRSs on an 11.2-mm pitch, were welded to a simulated tubesheet, two by EB welding and four by GTA welding. The test capsules were pressure and temperature cycled and then sectioned to evaluate the welds.³⁹ Early in CY 1979, a decrease in rod-to-rod pitch from 11.2 to 9.8 mm was considered. The resulting reduced working area and the necessity to lengthen the FRS terminals in order to attach the electrical connectors associated with the smaller pitch introduced additional complications to welding the cladding-to-tubesheet joint. Four test capsules — with the appropriate size FRS dummies — were fabricated on the 9.8-mm pitch to prove the practicability of welding the joint. Two of the capsules were welded by ORNL Metals and Ceramics Division using the GTA method; the capsules were successfully welded, and sectioning verified the good and acceptable quality of the welds.

The other two capsules were sent to EBW. The length of the terminal extensions above the tubesheet for the THORS bundle is 51 mm (2.0 in.).

The CFTL terminals (at that time) were 162 mm (4.0 in.) long. Some concern was expressed that the beam could not be focused at so large a distance; however, this was not the case and the welds were completed, sectioned by Metals and Ceramics, and found to be satisfactory (this in spite of some looseness in the joint fitup before welding). The EBW Company informed the project that it was capable of welding an item from a distance of 204 mm (8 in.).

The CFTL bundles are anticipated to be much longer and heavier than the THORS bundle and would exceed the length and weight limitations of the EBW facilities. The decision to go to the upflow core increased the length of the bundle substantially, exacerbated the problem, and eliminated all but a few EB welding firms. Of these firms, only one seemed agreeable to our using their facilities. However, the necessity for transcontinental transport of the bundles associated with the EB welding process (possibly several times per bundle), with all the opportunities for mishandling and damage such transport augurs, led the project to select the equally satisfactory GTA process. The GTA welding process can be performed in the CFTL site building; this capability should minimize transport and handling. Members of the Metals and Ceramics Division will train the site building welders in the application of the GTA process.

1.4.5.2 Electrical Connectors for Fuel Rod Simulators — H. C. Young and T. B. Conley

We continued evaluation of four candidate brazing techniques⁴⁰ for attaching the electrical connectors that join the two terminals in an individual FRS to the corresponding leads of the test bundle power supply. The need to cool the connectors, terminals, leads, and associated insulation to protect them from heat conducted from the tubesheet and generated electrically in the leads was broached.

Each FRS has two electrical connectors (and terminals), one connector for the center terminal and one for the annular terminal, as shown in Fig. 1.31. The four candidate techniques are

1. brazing both connectors manually using a gas torch (oxyacetylene);
2. brazing both connectors manually using a GTA torch;

Fig. 1.31. Electrical Connectors for Joining Fuel Rod Simulator (FRS) Terminals to Power Supply Leads. (a) Annular terminal. (b) Connector foot. (c) Central terminal connector. (d) Dummy FRS.

3. brazing the central connector manually with a GTA torch and the annular connector with a precision-head, fixture-located GTA torch;
4. brazing both connectors with a precision-head, fixture-located GTA torch.

The first three techniques were tested, and plans were made to test the fourth. The proof tests consisted of first brazing the electrical connectors by one of the above methods to dummy rods in a mockup (shown in Fig. 1.32) that simulates the close-packed FRS terminal region. After the brazing, the brazed connections were subjected to the design value of a_c , and the temperature and voltage drop for individual braze joints were monitored.

ORNL Photo 6769-79

Fig. 1.32. Tubesheet Mockup with Dummy Fuel Rod Simulators in Place.

Oxyacetylene brazing proved unsatisfactory because of severe oxidation of connectors and terminals. In addition oxidation could occur in the braze joint, increasing its electrical resistance and thus its heat generation.

The manually operated GTA technique was attempted next. Although a full mockup was brazed and electrically tested, the technique was rejected because the joint quality varied too much. The small working area available to the craftsman as indicated by Fig. 1.33 all but prohibited the positioning of the arc in just one location long enough to melt the braze filler metal. Any random movement of the arc resulted in damage to several connectors and FRS terminals, an intolerable situation for actual bundle assembly.

ORNL Photo 6766-79

Fig. 1.33. Crowded Area Facing Craftsman in Connector Area.

Brazing the annular connector with the precision-head GTA and the central connector with the manual GTA torch was somewhat better but still not completely satisfactory. This attempt made by the ORNL Metals and Ceramics Division revealed the need for a relatively accurate fixture for locating both torches. The first few joints in the mockup were easily done, but, as the number of connectors increased, the visibility and the access decreased. Occasionally the exact position of the tip of the precision head was unknown and spurious arcing occurred. When the brazed mockup was tested electrically it showed no significant difference in temperature or voltage drop from the previous mockup.

Work began on the fourth technique using precision-head GTA torches with fixtures devised by the ORNL Metals and Ceramics Division. A mockup was fabricated with more detail of the connector area and of the work

situation facing the craftsmen than was given in the previous mockups. The mockup has dummy rods representing the 8-mm-diam FRS with terminals 165 mm long spaced on a rod-to-rod pitch of 12 mm in the tubesheet. Brazing will be done in the near future.

Operation of the CFTL will expose the tubesheet to helium at temperatures approaching 350°C, and at full bundle power the FRS will carry at least 125 A ac. Thus the electrical conductors in the connector region will operate at temperatures that may damage the electrical insulation system unless cooling is provided. To estimate the amount of cooling required, the brazing mockups were exposed to different air flows while design current was applied to the electrical conductors, and the mockup tubesheet was heated by a clamshell heater to 260°C. A temperature of 260°C was chosen rather than 340°C to account for the cooling of the inside of the tubesheet by the flowing helium in a CFTL test bundle.

In the first test a stream of plant compressed air at high velocity (but low volume) was blown over the terminals and provided satisfactory cooling. Without cooling, the temperature of the terminals reached about 320°C, but the use of the plant air reduced the temperature to as low as 150°C. Concern was voiced over the possible loss of plant air during CFTL operation and the effect on the whole region of the unreliable cleanliness of plant air. Thus the forced convection of room air with an electric blower was tested. The low-velocity high-volume output of the blown room air appeared to cool the terminals about as effectively as the high-velocity plant air.

It may be advantageous to back up the power supply to the blower motor with an automatic connection to the CFTL site building backup diesel-generator power supply, which is activated whenever the primary power to the site building fails. To determine the thermal effect on connector temperature of the 19-s delay in the automatic switchover from primary to diesel electric power, the mockup was brought to operating conditions and the current to the dummy rods and the blower was cut off. The resulting temperature rise of about 3°C that occurred during the 19 s the blower is shut down awaiting switchover was deemed to be harmless to

the insulation. Loss of cooling air flow to the electrical connector region will require automatic alarm and safety action to protect the connections and insulation from damage.

The close spacing of the FRSs and their electrical connectors requires that these parts be insulated from one another. The area of main concern is the gap between the outer connector braze joints of adjacent FRSs as shown in Fig. 1.33. On the 11.2-mm pitch design (downflow core) this gap was only 0.64 mm (25 mils); on the 12.0-mm pitch design (upflow core), this gap increased to 1.25 mm (50 mils). The present configurations of the two FRS connectors and their electrical insulation are shown in Fig. 1.34.

1.4.6 Test Bundle Procurement — H. C. Young

Figure 1.35 shows a summary schedule for the design, procurement, fabrication, and assembly activities for test bundle AG1. Because of a lack of funding in FY 1980 and changes in bundle requirements by GA (the cladding length was changed from 2420 to 3065 mm and the diameter was changed from 7.46 to 8.00 mm), the completion of bundle AG1 has slipped approximately eight months from the previous⁴¹ schedule.

The claddings for the FRSs for bundle AG1 were procured by GA and were to have been roughened by GA before shipment to ORNL. Although UCC-ND requested that acceptance tests for the claddings should include an ultrasonic examination, the GA cladding specification did not have the requirement. However, upon receipt of the cladding GA contracted with a nondestructive testing laboratory to perform the ultrasonic examination of nearly 100 claddings. After the examination results were found to be inconclusive, all the claddings were shipped to ORNL to evaluate possible use in bundle AG1. The original requested date for GA to deliver the roughened claddings to us was September 1979. If the ORNL evaluation does not yield a sufficient quantity of acceptable claddings for bundle AG1, a reorder of new claddings will add up to eight months to the schedule.

The small-diameter TCs for installation inside the FRSs for bundle AG1 were ordered January 1979 for a requested delivery in September 1979 that was acceptable to the TC manufacturer. The order specified 0.5-mm-diam, Inconel-600-sheathed, MgO-insulated, isolated-junction,

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Fig. 1.34. Configuration of the Inner and Outer Electrical Connectors Joining Rod Terminals to Power Supply Leads. Insulation details shown also.

SCHEDULE FOR CFTL FUEL TEST BUNDLE AG 1
37 ROD, 12.0 mm ROD SPACING
UPFLOW AND RESTRAINTS

DESIGN
 PROCURE
 FABRICATE

Fig. 1.35. Schedule for Design, Construction, Assembly, Installation, and Operation of the First CFTL Test Bundle, AG1.

type K TCs for the bundle. Subsequently, the order was changed to reflect the increases in TC lengths caused by the increase in FRS length and increase in the number of TCs per FRS from four to six.

Experience with small-diameter TCs in other FRSs indicated the need and the room to improve TC life and reliability by minimizing the residual cold work in thermoelements, minimizing the grain size of the thermoelements, eliminating oxides from the MgO insulation, and then fabricating under an inert gas. As a consequence our engineers have approved a drawing and annealing schedule for minimizing grain size that was evolved with the full cooperation of the manufacturer. It is expected that the TCs will be delivered by April 1980.

Components being procured for the bundle AGI FRSs include copper tubular and rod terminals, variable-width heating elements made with Kanthal A-1 alloy, nickel central current return rod leads and tubular leads, the nickel-rod-to-heating-element end plugs, and inner and outer BN preforms.

All copper components are on hand as are the nickel end plugs, the nickel tubular terminals, and the heating elements. Nickel rod was received but did not meet specifications and was returned. Although the delivery of new nickel rod is uncertain, we believe its delivery will not affect the schedule. It should be on hand for fabricating the CFTL FRSs. Eagle-Picher, the BN preform fabricator, will receive automatic pressing dies in early CY 1980 and will supply preforms as needed on a satisfactory schedule.

Welding and brazing processes have been developed for joining the components, and the joining operations will be coordinated with FRS fabrication.

General Atomic is to supply the hex liner, grid spacers, and support rods. It has placed these items on order with spare parts as requested by ORNL. Delivery is expected in the third quarter of FY 1980. The Grayloc flange that will be used as the bundle tubesheet was ordered along with other Grayloc flanges required for the CFTL loop in the spring of 1979. Delivery has slipped several months and the present delivery date is July 1, 1980. Bundle instrumentation has not yet been ordered.

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2. GCFR SHIELDING AND PHYSICS

D. T. Ingersoll

2.1 INTRODUCTION — D. T. Ingersoll

Activities performed in the GCFR Shielding and Physics Program during 1979 include completion of the grid-plate shield design verification experiment postanalysis; completion of the radial blanket and shield experiment and associated preanalysis, and initiation of postanalysis; design support analysis for preliminary upflow shield designs; preliminary feasibility assessment of the fast mixed-spectrum reactor concept; and analysis of critical experiments performed at the Swiss Federal Institute for Reactor Research (EIR), Würenlingen, Switzerland.

2.2 GRID-PLATE SHIELD DESIGN CONFIRMATION EXPERIMENT — C. O. Slater, S. N. Cramer, and D. T. Ingersoll

A few final calculations for the postanalysis of the Grid Plate Shield Design Confirmation Experiment were performed to aid in the interpretation of the experiment as it related to the GCFR design. Following those calculations and discussions, the final report on the analysis of the experiment¹ was completed and issued. The final conclusions differed slightly from those reported in last year's progress report. In particular, the effect of neutron streaming on leakage from the fuel pins was only 9 to 11% for void fractions between 44.5 and 60.2%. The more significant contribution to the observed differences was due to different fuel densities. The effect of streaming was essentially constant for a wide range of void fractions. Secondly, varying the gap width between 3.175 and 12.7 mm caused a 28% increase in the fast neutron flux but only about an 8% increase in the total flux. These results were interpreted to mean that streaming has a significant effect on the fast neutron exposure to the grid plate, but a small overall effect on the leakage from the axial blanket. Thirdly, the transport through the grid plate was sensitive to quadrature. At the exit of the grid plate penetrations, a biased quadrature overpredicted results by up to a factor

of 2.2, while a lower order symmetric S_{10} quadrature greatly underpredicted results. However, both quadrature sets gave essentially the same results a short distance into the grid plate. We estimated that the lower order quadrature underpredicts fluxes in the GCFR upper plenum (downflow design) by factors of 2.5 to 4.0. Since design constraints had been met by large margins in the initial calculations, the inclusion of such bias factors in the calculated responses posed no problems for the shield design.

In summary, the methods and data used in the analysis of the GCFR design are adequate for calculating (1) axial neutron streaming in the GCFR core and axial blanket for a range of fuel-pin and subassembly spacings and (2) the peak fast-neutron exposure to the grid plate. On the other hand, depending on the quadrature used, one could have overly conservative or severely underpredicted flux levels in the upper plenum and, thus, an incorrect shield design.

A paper on the analysis was presented at the American Nuclear Society Summer Meeting in Atlanta, Georgia.² This resulted in a request that a full paper be submitted to *Nuclear Technology*, and that paper is in preparation.

2.3 RADIAL BLANKET AND SHIELD EXPERIMENT — D. T. Ingersoll and F. J. Muckenthaler

2.3.1 Prealysis

An integral experiment was designed for the verification of radiation transport methods and nuclear data used for the design of the radial shield for the proposed 300-MW(e) GCFR. The scope of the experiment was chosen to include a thorium oxide radial blanket mockup as well as several shield configurations in order to reduce the uncertainties in the calculated source terms for the radial shield, and to reduce the uncertainties in the calculated radiation damage to the prestressed concrete reactor vessel (PCRVR). Additionally, the measurements were intended to bound the uncertainties in calculated gamma-ray heating rates within the blanket and shield.

Considerable preanalysis of the experiment was performed to solve design and fabrication problems. The analysis consisted of one-dimensional neutron and gamma-ray transport calculations in slab geometry representing the experimental configurations. Also, sensitivity calculations were performed for several of the detectors to be used in the experiments. Results showed that the detectors were highly sensitive to the nuclear cross sections of the materials that are of primary importance. Based on the intended objectives and the results of the preanalysis, a detailed measurement schedule was prepared. This schedule includes a description of configurations, detectors, and detector positions. The significant results from the preanalysis and the measurement specifications were published.³

2.3.2 Measurements

The integral experiment was performed from March through September, 1979, and consisted of measurements of the neutron spectra, gamma ray spectra, and neutron flux behind successive mockups of (1) the spectrum modifier, (2) the radial blankets of either ThO_2 or UO_2 , (3) inner and alternate inner radial shields, and (4) the outer radial shield. The gamma ray heating was measured in situ at selected locations within the blankets and shields.

The design of the experiment was similar to previous shielding experiments performed at the Tower Shielding Facility for other programs. The mockups consisted of 1.50 by 1.50-m slabs of material, one following the other, placed perpendicular to the Tower Shielding Reactor II (TSR-II) neutron beam centerline. Figure 2.1 shows the experimental arrangement for the full radial shield mockup, including spectrum modifier, ThO_2 blanket, inner and outer radial shields, the steel liner, and PCRV. The slabs in the configuration were surrounded by both lithiated paraffin and concrete. The concrete served to reduce background at the detector, while the lithiated paraffin minimized the contribution of neutrons that reflect from the concrete back into the mockup. Included in the original program were mockups designed to study shield heterogeneity effects and deep

Fig. 2.1. A Schematic of GCFR Radial Blanket and Shield Experiment (Spectrum Modifier + ThO₂ Blanket + Inner and Outer Radial Shields and PCRV).

penetrations into concrete. Priorities for these two studies were less than those of the lengthy radial shield measurements and could not be included in this experiment.

Detectors used were the NE-213 and hydrogen-filled proton recoil detectors for neutron spectra, NE-213 for gamma ray spectra, Bonner balls for the integral neutron flux measurements, and thermoluminescent dosimeters (TLDs) for gamma ray heating within the shields. Flux and spectra measurements were obtained behind the configuration on the beam centerline, while the TLDs were dispersed in voids throughout the mockups.

The ThO_2 blanket mockups consisted of four type 304L stainless steel containers filled with ThO_2 powder with densities from 7.10 to 7.37 Mg/m^3 . Two of the containers were approximately 15 cm thick, representing full blanket rows, whereas the other two were half that thickness to represent half rows. The shield configurations consisted of slabs of type 304 stainless steel, solid graphite, and mixtures of graphite and B_4C powder (containing on the average 27% B and packed to a density of 1.4 Mg/m^3) within stainless steel containers.

The configurations are listed completely in Table 2.1. The experiment and resulting measurements will be reported.⁴

2.3.3 Postanalysis

The final or postanalysis of the integral experiment began in June 1979 and will continue through September 1980. The initial emphasis was on the analysis of the blanket results, especially the neutron transmission data for ThO_2 . A complete analysis of the experiment will consist of one-dimensional (1-D) fine-group calculations and two-dimensional (2-D) semifine-group calculations of the neutron and gamma-ray fluxes throughout the experiment configurations. The calculated fluxes are then folded with energy-dependent response functions to predict the observed data from the Bonner balls and TLDs and are compared directly with the reduced data from the spectrometer measurements. The postanalysis will be concluded by relating the observed data and analysis conclusions to the current GCFR radial shield design.

A major portion of the postanalysis, which was completed in FY 1979, is described in a paper presented at the Fifth International Conference on Nuclear Cross Sections for Technology.⁵ The most significant result

Table. 2.1. Configurations and Measurements for GCFR Radial Blanket and Shield Experiment

Configuration	Priority	Description ^a	Measurements ^b
I-A	IA	Spectrum modifier	BB, S
I-B	IA	Spectrum modifier + 1/2 ThO ₂ blanket row	BB
I-C	IA	Spectrum modifier + 1 ThO ₂ blanket row	BB
I-D	IA	Spectrum modifier + 2 ThO ₂ blanket rows	BB, S, TLD
I-E	IA	Spectrum modifier + 3 ThO ₂ blanket rows	BB, S, TLD
I-F	IA	Spectrum modifier + 3 ThO ₂ blanket rows + reflector	TLD
II-A	IA	Spectrum modifier + 3 UO ₂ blanket rows	BB, S, TLD
II-B	IA	Spectrum modifier + 3 UO ₂ blanket rows + inner radial shield	BB, S
III-A	IB	Spectrum modifier + 3 ThO ₂ blanket rows + 1 row inner shield	BB, S
III-B	IB	Spectrum modifier + 3 ThO ₂ blanket rows + 2 rows inner shield	BB, S
IV-A	IC	Spectrum modifier + 3 ThO ₂ blanket rows + 2 rows inner shield + 1/3 outer radial shield	BB
IV-B	IC	Spectrum modifier + 3 ThO ₂ blanket rows + 2 rows inner shield + 2/3 outer radial shield	BB
IV-C	IC	Spectrum modifier + 3 ThO ₂ blanket rows + 2 rows inner shield + outer radial shield	BB, S
IV-D	IC	Spectrum modifier + 3 ThO ₂ blanket rows + 2 rows inner shield + outer radial shield + PCRV	BB, TLD
IV-E	IC	Spectrum modifier + 2 rows inner shield + outer radial shield	BB, S
V-A	ID	Spectrum modifier + 3 UO ₂ blanket	BB
V-B	ID	Spectrum modifier + 3 UO ₂ blanket + 1/3 alternate inner radial shield	BB, S
V-C	ID	Spectrum modifier + 3 UO ₂ blanket + 2/3 alternate inner radial shield	BB, S, TLD
V-D	ID	Spectrum modifier + 3 UO ₂ blanket + alternate inner radial shield (I)	BB, TLD
V-E	ID	Spectrum modifier + 3 UO ₂ blanket + alternate inner radial shield (II)	BB, TLD

^aInner and outer radial shields refer to conceptual shielding configuration I design.

^bBB = Bonner ball, S = spectrometer, TLD = thermoluminescent dosimeter.

obtained thus far is a comparison of ENDF/B-IV and ENDF/B-V data for ^{232}Th . Figure 2.2 shows the measured high-energy neutron spectrum emerging from 45 cm of ThO_2 compared with 1-D analytic predictions of the observed spectra. Although the calculations have been artificially normalized to the area under the NE-213 spectrum, it is clear from the comparisons that the calculation that used ENDF/B-V thorium data gives an overall better agreement with the measurement.

The improvement of the thorium cross-section evaluation for ENDF/B-V is further substantiated by a comparison of calculated and measured attenuation factors for a single slab of ThO_2 . The attenuation factors were determined by calculating for each detector the ratio of the response before and after the 15-cm ThO_2 slab was placed in the configuration. This "In/Out" ratio represents the attenuation of the integrated response of each detector. As seen in Table 2.2, the calculated attenuations are low compared with the measurements; however, the calculation using ENDF/B-V-based thorium shows an improvement. Also, adding a buckling correction to the 1-D slab calculations improves the comparison. The higher energy responses show the best agreement, which suggests that the ThO_2 total cross section appears adequate at higher energies but too low at lower energies.

A similar analysis for a 15-cm slab of boronated graphite, which was placed behind the ThO_2 , is shown in Table 2.3. The nearly uniform 13% difference between the measured and calculated attenuations is likely due to the 1-D approximation and/or uncertainties in the slab density.

2.4 SHIELDING ANALYSIS IN SUPPORT OF THE UPFLOW GCFR DESIGN —

C. O. Slater, E. T. Tomlinson, L. R. Williams, and D. A. Reed

Shielding Design analysis was deferred for a short period in 1979 until a decision could be made on the direction of coolant flow and a preliminary shield design could be produced. For the preliminary calculations, the core was assumed to be essentially the same as before with a few dimensional changes. This enabled a faster evaluation of the shield design.

Fig. 2.2. Comparison of Normalized 1-D Calculations and Measured Spectrum Behind Spectrum Modifier and Three-Row ThO_2 Blanket Mockup. Calculations used ENDF/B-IV thorium data (left) and ENDF/B-V thorium data (right).

Table 2.2. Neutron Attenuation Through 15 cm ThO₂

Detector ^a	Attenuation (In/Out)		
	Measured	ENDF/B-IV	ENDF/B-V ^b
Three-inch BB (epithermal)	3.70	2.85	3.04 (3.17)
Six-inch BB (total)	4.24	3.46	3.69 (3.87)
Ten-inch BB (fast)	4.39	3.92	4.19 (4.40)
NE-213 (1.5-15 MeV)	6.15	5.10	5.42 (5.60)

^aBB = Bonner ball.

^bValues in parentheses represent attenuations calculated with a finite buckling correction applied.

Table 2.3. Neutron Attenuation Through 15 cm Boronated Graphite^a

Detector ^b	Attenuation		
	Measured	Calculated	C/E
Three-inch BB (epithermal)	10.2	8.91	0.87
Six-inch BB (total)	9.24	8.45	0.91
Ten-inch BB (fast)	8.87	7.68	0.87
NE-213 (1.5-15 MeV)	5.14	4.47	0.87

^aA mixture of graphite and B₄C powder with 26 wt % B.

^bBB - Bonner ball.

The fission density from the previous calculation was altered to fit the modified core and blankets. Heterogeneous and homogeneous cell calculations and a full homogeneous model calculation were performed with the fission density used as a fixed volumetric source. The full homogeneous model yielded internal boundary sources at three boundaries for use as modified sources for the radial and the upper and lower plenum calculations. The boundary sources were corrected for streaming by scale factors that would give heterogeneous-to-homogeneous leakage ratios equal to those obtained by comparing homogeneous and heterogeneous cell-calculation results. The ratios for radial streaming were near 1.0, so a conservative scale factor of 1.25 was applied for all groups. Scale factors for the axial boundary sources were quite large in some instances, averaging 8 to 10 for all groups. The initial scale factors, obtained by taking the ratio of heterogeneous and homogeneous cell leakages into the upper and lower plena, were adjusted so that the modified internal axial boundary sources would give the same or conservative leakages into the plena. The cell calculations used an S_{12} (96-angle) symmetric quadrature, while the full homogeneous calculation used a symmetric S_6 quadrature. All calculations used 76 groups (51 neutron and 25 gamma-ray) and P_3 cross-section expansion.

Figure 2.3 shows an isoplot of the total neutron fluence (24 full-power years) in the lower portion of a fuel subassembly as calculated by using a heterogeneous cell model. The drawing is actually inverted for the calculation. The grid-plate webbing is represented by the large rectangular area in the upper right of the figure. The initial 10 cm along the axis is the pressure equalization system plate, which is constructed of the same material as the grid plate. If bias factors from the Grid Plate Shield Design Confirmation Experiment were applied, the peak neutron fluence in the grid plate would be about $7 \times 10^{21}/\text{cm}^2$. Perkins and Cerbone⁶ studies showed fluences of 2.4×10^{22} to $2.8 \times 10^{22}/\text{cm}^2$ required to reach the lower bound estimate of 10% residual total elongation (the criterion imposed on the grid plate). A more conservative estimate places the fluence at about $10^{22}/\text{cm}^2$. Thus, the

Fig. 2.3. Isoplot of the 24-Full-Power-Year Neutron Fluence in a Heterogeneous Cell Mockup of the Lower Portion of the Upflow GCFR Full Subassembly. Fluences are in neutrons/cm².

fluence in the grid plate satisfies the most conservative criterion for the fluence and, therefore, radiation damage should not be the limiting factor on the grid plate lifetime.

Figure 2.4 shows an isoplot of the total neutron fluence for the full homogeneous model calculation. Note the factor of 10 difference in the total fluence at the bottom of the grid plate (lower left in Fig. 2.4) compared with that at the top of the grid plate in Fig. 2.3. The average heterogeneous-to-homogeneous cell leakage ratio was found to be about 11 for the lower regions. Internal axial boundary sources at $Z = -186$ cm and $Z = 184$ cm were modified so that calculations for the upper and lower regions had centerline fluences at the plenum interface comparable to those for the heterogeneous cells. Along with the radial source at $R = 158.3$ cm, these sources were input as necessary into the upper and lower plenum calculations and the radial shield calculation. These calculations are described in the following sections. More detailed results may be found in ref. 7.

Fig. 2.4. Isoplot of the 24-Full-Power-Year Neutron Fluence in the Homogeneous Mockup of the Upflow GCPR Midlevel Regions. Fluences are in neutrons/cm².

2.4.1 Upper Plenum Shields

A DOT-IV calculation was performed on a 2-D R - Z mockup of the upflow GCFR from 34 cm below the core midplane through a portion of the top plug of the reactor (892 cm). Included in the geometry was a mockup of the main circulator inlet duct. Radiation levels within this duct model were conservative when compared with those for the actual duct design. The group structure consisted of 51 neutron and 25 gamma-ray groups. The quadrature set was S_6 symmetric and the cross sections were expanded to P_3 moments. Over 15,000 mesh points were used in modeling the geometry. Boundary sources at $Z = 184$ cm and $R = 158.3$ cm were used, and the region surrounded by them was made a pure absorber.

A typical result from this calculation is the total neutron fluence (24 full-power years) isoplot shown in Fig. 2.5. The results are indicative of the neutron flux levels around components or penetrations in the upper plenum. As will be explained in the next section, these results should be increased a factor of 3.7 because of a redefinition of the upper axial boundary source. All radiation criteria are met except for the total neutron fluence in the core barrel; the $10^{21}/\text{cm}^2$ limit is exceeded over a portion of the core barrel. The PCRV liner exposure is nearly two orders of magnitude below the criterion. Finally, note that unlike the previous downflow design,⁸ where the gap between the radial shield and the wraparound shield was near the lower shield, the present top head shield is a much larger distance from the flow-through gap between the radial and wraparound shields. One thus notes relatively less streaming through the gap.

2.4.2 Radial Shield

The radial shield concentrated mainly on the portion of the reactor with Z -levels between the bottom of the grid plate and the top of the exit shield region. Like the upper plenum shield calculation, this calculation was also performed with DOT-IV using 51 neutron groups, 26 gamma-ray groups, S_6 quadrature, and P_3 cross-section moment expansion. Three modified internal boundary sources were included to allow for possible contributions from the strong upper and lower axial

Fig. 2.5. Isoplot of the 24-Full-Power-Year Neutron Fluence in the Upper Regions of the Upflow GCFR. Fluences are in neutrons/cm².

sources at the centerline. However, a crude mesh was chosen for the portion of the source between $R = 0$ and $R = 158.3$ cm, where the radial source was located. The axial boundary sources were located at $Z = -186$ cm and $Z = 184$ cm. The region surrounded by the three sources was represented as a pure absorber.

Figure 2.6 shows the zone geometry for the 2-D mockup and indicates the positions of the three boundary sources. Figure 2.7 shows an isoplot

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Fig. 2.6. Zone Geometry for a Midlevel Mockup of the Upflow GCFR for the Calculation with the Adjusted Boundary Sources.

Fig. 2.7. Isoplot of the 24-Full-Power-Year Neutron Fluence in the Midlevel Regions of the Upflow GCFR for the Case with Axial Boundary Sources Corrected by Streaming Factors Based on Heterogeneous-Cell-to-Homogeneous-Cell Leakage Ratios. Fluences are in neutrons/cm².

of the total neutron fluence (24 full-power years) as determined by the calculation. The results show a somewhat higher total fluence at the core midplane than did the upper plenum calculation but in general lead to the same conclusion. The only potential problem appears to be in the core barrel near the core midplane.

A second calculation for this mockup was performed for a different set of axial boundary sources. Instead of comparing heterogeneous and homogeneous cell leakages to get the streaming correction factors, the leakages from the heterogeneous cell were compared with those along the

centerline of the full homogeneous model. These factors should be more indicative of the scale factors needed for the homogeneous model calculation, using S_5 quadrature, to give results comparable to the heterogeneous cell calculation using S_{12} quadrature. As explained earlier, the ratios were adjusted such that when they were applied to the internal boundary source, the new calculation would give results at the top of the exit shields or the bottom of the grid plate comparable to those obtained in the heterogeneous cell calculations. An isoplot of the total neutron fluence results from this calculation is shown in Fig. 2.8.

Fig. 2.8. Isoplot of the 24-Full-Power-Year Neutron Fluence in the Midlevel Regions of the Upflow GCFR for the Case with Axial Boundary Sources Corrected by Streaming Factors Based on Heterogeneous-Cell-to-Full-Homogeneous-Model Leakage Ratios. Fluences are in neutrons/cm².

It shows the somewhat higher results at the top of the exit shields and the bottom of the grid plate as compared with the results of Fig. 2.7. Examination of the leakages showed an overall factor of 3.7 increase in the neutron and gamma-ray leakage into the upper plenum. In the lower plenum, the neutron leakage increased a factor of 3.0 while the gamma-ray leakage increased a factor of 2.8. Thus, the upper and lower plenum results presented in the previous and succeeding sections should be increased by factors of 3.7 and 3.0, respectively.

2.4.3 Lower Plenum Shields

The lower plenum calculation likewise used 51 neutron groups, 25 gamma-ray groups, S_6 quadrature, and P_3 cross-section moment expansion. Boundary sources at $R = 158.3$ cm and $Z = -186$ cm were used, and the region surrounded by them was made a pure absorber. This mockup, like the upper plenum mockup, also included the outlet duct from the main helium circulator. Such a duct mockup should give conservative radiation levels within the duct and the PCRV surrounding it. Figure 2.9 shows an isoplot of the total neutron fluence in the lower plenum. It shows all fluence criteria being met everywhere. In contrast to the upper plenum shield, the flow-through gap between the radial and wraparound shields is much closer to the lower shielding. As a consequence, the streaming through the helium flow-through gap is much more noticeable than in the upper plenum. As mentioned in the previous section, a factor of 3.0 should be applied to these results for the alternate source consideration.

2.4.4 Additional Analysis

Work was begun on the analysis of very different upper and lower plenum shield designs for a 400-MW(e), two-loop GCFR. The new design consists of flow-through shields for the wraparound portion of the plenum shields, and the wraparound segments are now flush with the radial shield. The flow-through paths considered were slits with single bends (chevron) and double bends (herringbone) with widths of 5.08, 7.62, and 10.16 cm. Initial analysis concentrated on calculating the streaming effects of the various gaps. No results have yet been obtained. This analysis along with the full shield analysis will be continued into the next year.

Fig. 2.9. Isoplot of the 24-Full-Power-Year Neutron Fluence for the Lower Regions of the Upflow GCFR. Fluences are in neutrons/cm².

2.5 GCFR REACTOR PHYSICS ANALYSIS — T. J. Burns, J. R. White, and D. T. Ingersoll

2.5.1 Reactor Physics of FMSR

The Fast Mixed-Spectrum Reactor (FMSR) concept has been proposed (by Brookhaven National Laboratory) as a means of alleviating certain nonproliferation concerns relating to civilian nuclear power. In particular, the concept is envisioned as operating on a natural uranium feed (after initial startup), thus eliminating the need for fuel reprocessing. The primary basis for the concept involves the production, in situ, of the fissile material from the fertile feed material required for criticality. A preliminary study directed at assessing the neutronic feasibility of the concept has been performed at ORNL.

The study evolved through four major phases. Scoping calculations were first performed with an available GCFR cross-section library and a reference FMSR calculational model. These initial calculations indicated the need for a more detailed broad-group representation of the cross sections than that utilized in the GCFR library due to the large spectral and spatial gradients inherent in the FMSR design. Further, large differences in the fission product cross sections relative to those employed in the reference calculation were observed. Hence, spatially dependent broad-group nuclear data, including a detailed fission product cross section, were generated specifically for the FMSR. The next phase of the study involved the calculation of the major physics parameters relating to the FMSR using these new cross sections. Multicycle calculations using the new FMSR cross sections indicated that alternate designs (relative to the benchmark) would be required to meet criticality requirements. Finally, some possible design variations and modeling questions were briefly investigated.

No definitive conclusion as to the feasibility of the FMSR concept can be made from the preliminary analysis, however. Specifically, because of the large number of potential design changes that can be made, further study is required to determine feasibility. This investigation, however,

has indicated several areas in which further work is required before the basic feasibility of the concept can be either proved or disproved. The detailed results of this investigation are documented.⁹

In addition to the above physics feasibility study, a brief investigation of the effects of large fission product inventories in the FMSR and an analysis of the effect of burnup on fission product nuclide distributions and effective cross sections were made. Results from this study indicate that care needs to be taken when modeling the effects of fission products on reactor performance. The large fission product inventory in the FMSR not only has significant direct reactivity effects, but also has a strong influence on power and flux distributions and breeding performance. Deletion of the fission products in a reference FMSR design resulted in a 5.4% reactivity gain at the beginning of life (equilibrium model) and about 4.2% gain on the new equilibrium cycle, with the difference being caused by the radial redistribution of the neutron flux.

The effect of burnup of effective fission product cross sections was also analyzed with somewhat surprising results. The final conclusion was that a one-lump fission product model is sufficiently accurate for reliable FMSR design studies. Although a multinuclide fission product set that explicitly accounts for the more important chains would be a more accurate representation, the added complexity and cost would outweigh the additional accuracy. We feel that the uncertainty in the one-lump model is tolerable for preliminary design calculations and probably lies well within the uncertainty of the individual fission product cross sections.

Finally, based on the above conclusions, a 50-group fission product library was generated. The library contains a single fission product lump for each of six spectral regions of a 1-D FMSR model.

The detailed results of this investigation are being reported.¹⁰

2.5.2 Analysis of EIR-PROTEUS Experiments

An extensive program of reactor physics experiments in GCFR fuel pin lattices¹¹ was completed recently at the PROTEUS critical facility located at EIR laboratory in Switzerland. The PROTEUS reactor consists of a central test zone surrounded by a uranium buffer and thermal driver

region. The test lattices included a $\text{PuO}_2\text{-UO}_2$ fuel region with internal and axial blankets of UO_2 , ThO_2 , and thorium metal. Detailed analysis of the thorium-bearing lattices has been performed at EIR and at ORNL to validate nuclear data and methods used for reactor physics analysis of advanced GCFR designs. Results of the analysis of reaction rate measurements made at the center of the $\text{PuO}_2\text{-UO}_2$ core have been presented earlier;^{12,13} however, a thorough investigation of reaction rate measurements in the axial blankets has just recently been performed at ORNL and indicates significantly different conclusions from the core center analysis.

The two primary configurations that have been analyzed by ORNL are (1) a $\text{PuO}_2\text{-UO}_2$ core with a ThO_2 axial blanket (Core 14) and (2) a $\text{PuO}_2\text{-UO}_2$ core with a thorium metal axial blanket (Core 16). The core center results¹⁴ for Core 14 and similar core center results for Core 16 showed that ENDF/B-IV thorium data underpredict the ^{232}Th capture and fission rates with calculated-to-measured (C/E) values ranging from 0.93 to 0.96 for the ratio of ^{232}Th capture to ^{239}Pu fission (C2/F9) and 0.88 to 0.90 for the ratio of ^{232}Th fission to ^{239}Pu fission (F2/F9). Also, the use of ENDF/B-V thorium data yielded a 7% lower capture rate and a 5% higher fission rate relative to ENDF/B-IV-based data. These results indicate that in the contributing energy range of 100 eV to 1 MeV, the current thorium capture data appear inadequate for GCFR fuel cycle analysis, since a 5 to 15% uncertainty in the thorium capture rate is compounded to yield an unacceptably large uncertainty in the calculated breeding ratio and fuel doubling time for a thorium-bearing reactor.

The core center analysis, however, considered thorium only in an infinitely dilute environment, and hence did not include resonance self-shielding effects. To investigate these effects, we compared reaction rate measurements and calculations in the ThO_2 and Th metal axial blankets of Core 14 and Core 16. The results from the Core 16 analysis are given in Table 2.4, which gives C/E values for three important reaction rate ratios at the center of the axial blanket. The reaction rates were calculated by use of several different cross-section data sets and both diffusion theory and transport theory methods. In general, the calculations and measurements show a much larger disagreement in the blanket than

Table 2.4. Comparison of Calculated Reaction Rate Ratios at the Center of the Axial Blanket of Core 16

Reaction Rate Ratio	Measured Value	Calculated-to-Measured Value (C/E)			
		EIR ^a Data	ENDF/B-IV Data		ENDF/B-V Thorium Data
			Transport	Diffusion	
C2/F9	0.1066	1.34	1.20	1.22	1.13
C8/F9	0.3969	0.56	0.89	0.85	0.75
F2/F9	1.28×10^{-3}	1.34	1.17	1.19	1.13

^aEIR data were derived from British FGL5 data library except for ²³²Th and ²³³U data, which were derived from ENDF/B-IV.

at core center. Also, the cross sections generated at ORNL from ENDF/B-IV and -V data files showed improved agreement relative to the cross-section data supplied by EIR. In contrast to the core center results, using ENDF/B-V thorium data improved the C/E values for both the capture and fission rates (relative to ²³⁹Pu fission rate).

A careful investigation of the relatively large discrepancies observed in the blanket calculations concluded that modeling and methods uncertainties were the dominant factors, and consequently very little useful information regarding the adequacy of the nuclear data could be obtained from the analysis. In particular, we felt that the procedure for collapsing fine group cross sections to the ten broad groups used in the two-dimensional calculations seemed inadequate. The collapse was performed by using flux spectra determined from a 1-D calculation in the axial direction along the reactor centerline. While this procedure gives an inadequately representative weighting spectrum at the core center, the spectrum calculated in the axial blanket lacks a significant low-energy component due to the thermal driver region, which surrounds the test zone. Figure 2.10 gives the groupwise contribution of several reaction rates calculated in the axial blanket and shows that the lower energy groups are significant in the blanket region. This large sensitivity of the reaction

Fig. 2.10. Groupwise Reaction Rates at the Center of the Thorium Metal Axial Blanket. Calculated for Core 16 of the EIR-PROTEUS experiments.

rates to the low-energy flux, combined with the inadequate weighting of the low-energy broad-group cross sections, amply explains the disagreements between the calculated and measured reaction rates in the axial blanket.

Therefore, unlike the core center analysis, which showed the need for improved thorium capture data, the axial blanket analysis has shown the need for a more sophisticated method and improved modeling of the test reactor. It is expected that the C/E values in the axial blanket could be noticeably improved if either a better procedure for cross-section collapsing were used such as a fine-group 2-D calculation of the weighting spectra, or a finer group structure were used for the broad-group 2-D calculations.

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3. GCFR PRESSURE VESSEL AND CLOSURE STUDIES

D. J. Naus, J. R. Dougan, and G. C. Robinson

3.1 INTRODUCTION

This program provides technical support for the design and development of the GCFR prestressed concrete reactor vessel (PCRv). Current and completed studies have consisted of analytical and experimental evaluations of the closures for the steam generator cavity and the central core cavity. Figure 3.1 is a cross section of the PCRv showing these two types of closures.

Three principal activities of our program in response to ASME Code requirements are analyses and tests of (1) 1/20- to 1/15-scale closure plug models, (2) 1/4-scale closure plug models, and (3) a 1/10-scale model of the complete PCRv. The first of these activities has as primary objectives the determination of the margin of safety and the structural response due to overload. In addition, although limited by the relatively small scale, experimental data in the elastic regime are obtained for validation of design analyses. The second of these activities is intended to provide detailed confirmation of design methods employed by the designer by using a different computer code from the designer and by obtaining precise experimental strains and displacements at critical locations. The third activity is intended to obtain the margin of safety and the structural response due to overload for the complete PCRv.

The planning and execution of each of these activities have been premised on the schedules and designs of the General Atomic Company (GA) in order to integrate into the overall GCFR development schedule. Perturbations on GA's PCRv design as a consequence of technological influences have a direct and immediate impact on the ORNL program. During the previous reporting period efforts were initiated on the 1/15-scale central reactor cavity closure plug model and on the procurement of a 1/4-scale model test fixture to accommodate models of the steam generator cavity closure and the central core cavity closure. Although these activities

Fig. 3.1. Vertical Cross Section of the PCRV for the 300-MW(e) GCFR Demonstration Power Plant.

had progressed through the design stage to the procurement of components and materials, these efforts were interrupted and stopped because of significant design changes to the prototype PCRV. As a consequence ORNL efforts were redirected to implement a test of a 1/20-scale central core cavity closure plug model. Construction work on the test cell and pressurizing equipment to test 1/4-scale closure models and 1/10-scale complete PCRV models was completed.

3.2 1/20-SCALE CENTRAL CAVITY CLOSURE PLUG MODEL

Figure 3.2 shows the top plan and a vertical cross section of the 1/20-scale central cavity closure plug model. The prominent features of this design are the close-pitched array of penetration tubes (55), the segmented gusset-bearing plate assemblies (44), and the intermeshed reinforcing steel. In addition to the conventional objectives of margin of safety and structural response due to overload, the designer requested

Fig. 3.2. Plan and Vertical Section of 1/20-Scale Central Core Cavity Model (Dimensions in Centimeters).

that a particular effort be given to instrumenting the model so that concrete strains in the elastic regime could be obtained for the innermost tube ligament at midheight and at the top surface and for the zone immediately underneath the gusset-bearing structure. Linear scaling of the model dimensions and duplication of the mechanical properties of the materials from prototype to model have been sought in every part but one. Analysis indicates that the penetration tubes would fail from overload before failure of the structure as an entity if all mechanical properties were duplicated. Rather than increase the wall thickness of the tubing to obtain an acceptable overload test, the designer requested that a high-strength material substitution be made for the penetration tubes that would not only permit a valid overload test but would also permit valid strains to be monitored on the penetration top surface ligament area.

3.2.1 Strain Gage Development

As previously indicated, GA requested that the circumferential strain at the middepth of the concrete and at the midpoint of the concrete ligament between the center penetration tube and an adjacent penetration tube be measured as a function of load. Since this space is very confined, having an approximately 6-mm radial dimension, no commercial device was identified as being available for use. Two methods were developed for this purpose. In the first, a commercial weldable gage was split and splayed as shown on Fig. 3.3. In the second, three 1.58-mm polyimide-backed foil resistance gages were cemented in an X-Y-Z array to a rectangular graphite block having dimensions 2.3 by 2.3 by 6.4 mm. This assembly was then sealed with layers of epoxy and a proprietary low-melting Cerro alloy. Figure 3.4 from left to right shows this sequence of fabrication.

After exploratory tests indicated feasibility, a confirmatory test of these gaging concepts was performed. Figure 3.5 shows the development of the two types of gages applied to a 10.2-cm-diam by 40.6-cm-long microconcrete test cylinder. Table 3.1 provides placement and bridge completion data for each of the gages of Fig. 3.5. The flanges of the

Fig. 3.3. Weldable Gage with Flange Split and Splayed for Embedment in Concrete.

weldable gage were split and splayed as shown in Fig. 3.3 for embedment in the microconcrete. Figure 3.6 shows the positioning of the polyimide strain gages on the graphite bases relative to the loading direction of the test cylinder. The test cylinder was subjected to six cycles of loading to about half its ultimate strength and then on the seventh cycle was loaded to failure.

According to the theoretical development of Geymayer,¹ gages that were placed to measure the short dimension strain of the graphite bases (e.g., gages 11, 13, 14, and 16 on Fig. 3.6) would exhibit geometric effects relative to true strain. Comparison of the gage readings shown on Fig. 3.7 for gages 12 and 16 indicated no geometry effect for compressive strains. Because of poor bonding with the microconcrete, gages 11, 13, and 15, rather than providing good correspondence with gages 3, 7, 8, and 10 and compressometer DCDT-2 (see Figs. 3.7 and 3.8), indicated the Poisson's strain of the graphite base due to the compressive loading. The three-gage graphite-base array was therefore determined to be unreliable for tensile strains. On the other hand, the readings from weldable gages corresponded well with the readings from associated surface gages and compressometer DCDTs for both compressive and tensile strains.

Fig. 3.4. Display of the Fabrication Sequence Employed for Miniature Graphite-Base Concrete Embedment Gage.

Fig. 3.5. Development of 1/20-Scale Model Embedment Gage. Numbers in circles locate gages identified in Table 3.1.

Table 3.1. Gage Calibration: 1/20-Scale GCFR Closure Model Concrete Cylinder
(10.2-cm-diam by 40.6-cm-long) (1/4 bridge connection)

Gage ^a	Placement	Measurement Direction	Manufacturer	Catalog Number	Gage Factor	Resistance (Ω)
1	Surface	Axial	Micro Measurement	EA-06-10CBE-120	2.10	120
2	Surface	Circumferential	Micro Measurement	EA-06-10CBE-120	2.10	120
3	Surface	Axial	Micro Measurement	EA-06-10CBE-120	2.10	120
4	Surface	Circumferential	Micro Measurement	EA-06-10CBE-120	2.10	120
5	Pseudo surface	Axial	Ailtech	MG 128-11H	1.81	120
6	Pseudo surface	Circumferential	Ailtech	MG 128-11H	1.81	120
7	Pseudo surface	Axial	Ailtech	MG 128-11H	1.81	120
8	Pseudo surface	Circumferential	Ailtech	MG 128-11H	1.81	120
9	£ embedded	Axial	Ailtech	MG 128-11H	1.81	120
10	£ embedded	Radial	Ailtech	MG 128-11H	1.81	120
11	£ embedded	Radial	Magnaflux	MX PA-06-062CE-120	1.98	120
12	£ embedded	Axial	Magnaflux	MX PA-06-062CE-120	1.98	120
13	£ embedded	Radial	Magnaflux	MX PA-13-062GF-350	2.03	350
14	£ embedded	Radial	Magnaflux	MX PA-06-062CE-120	1.98	120
15	£ embedded	Radial	Magnaflux	MX PA-06-062CE-120	1.98	120
16	£ embedded	Axial	Magnaflux	MX PA-13-063GF-350	2.03	350

^aLocation shown in Fig. 3.5.

Fig. 3.6. Positioning of Polyimide Strain Gages on Graphite Bases Relative to Loading Direction.

The three-gage graphite-base strain readings, gages 12 and 16, gave good correspondence with readings from surface strain gages and compressometer DCDT-1. The three-gage graphite-base array was therefore judged to be satisfactory for use in the 1/20-scale model in zones anticipated to have compressive strains only; however, some redundant weldable gages should be placed at these locations for reliability.

Fig. 3.7. Load vs Strain for Gages 9, 11, 12, 13, 14, and 16 for the Seventh Load Cycle.

Fig. 3.8. Load vs Strain for Gages 1, 3, 5, and 7 and DCDT 1 and 2 for the Seventh Load Cycle.

3.2.2 Fabrication of Model

As previously discussed, all materials in the model have been selected and modified as necessary by heat treatment to obtain mechanical properties simulating those of materials used in the prototype, except for the tubing subassemblies. In simulation of ASME SA-537 class 2 plate or SA-508 class 2 forging, the outer liner and forging are being made from a section of ASTM A 106 grade B pipe that has been austenitized, quenched, and tempered. We used the pipe material and procedure employed in the steam generator closure models previously tested. The bottom liner and the bearing plate and gusset structures are being made from sections of ASTM A 533 grade B class 1 plate (austenitized, quenched, and tempered) to simulate ASME SA-537 class 2 plate. The tubing subassemblies that model the penetrations of the prototype are being made from Inconel 718, a proprietary age-hardenable alloy. Only the modulus of elasticity of the prototype penetrations, either ASME SA-106 grade B or SA-333 grade 6 piping, will be simulated in the model. The reinforcement steel, ASTM A 615 grade 60 in the prototype, is being simulated with 2 1/4 Cr-1 Mo welding wire. This choice was predicated by the ability of 2 1/4 Cr-1 Mo to give the proper mechanical property values after exposure to high temperatures associated with the furnace brazing of the tubing assemblies into the bottom liner.

The fabrication sequence being employed consists of machining and welding liner components; forming and welding reinforcement steel components; machining, brazing, and age hardening tubing subassemblies; and then assembling the composite by means of a final furnace braze. Figure 3.9 shows fixturing, a partial assembly, and fully assembled reinforcement steel components. Figure 3.10 shows fixturing for welding and machined components forming the gusset and bearing plate subassemblies. Figure 3.11 shows the tubing, end cap, and shear ring components ready for brazing and age hardening to form a penetration tube subassembly. Figure 3.12 shows a welding fixture for joining the gusset and bearing plate subassembly to the outer liner, a gusset and bearing plate subassembly, the outer liner in an intermediate machined condition, and the bottom liner with several anchors and a single penetration tube.

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Fig. 3.9. Reinforcement Steel Fabrication for 1/20-Scale Central Core Cavity Model.

PHOTO 8035-79

Fig. 3.10. Gusset and Bearing Plate Component Fabrication for 1/20-Scale Central Core Cavity Model.

PHOTO 8036-79

Fig. 3.11. Penetration Tube Fabrication for 1/20-Scale Central Core Cavity Model.

PHOTO 8034-79

Fig. 3.12. Components Assembly for 1/20-Scale Central Core Cavity.

3.2.3 Model Instrumentation

As noted previously, the primary purposes of instrumentation are monitoring the progression of failure and validation of computer programs; that is, those programs used by GA primarily for design analysis and those programs by ORNL primarily for failure mode and ultimate load analyses. Figure 3.13 and Table 3.2 provide the function, type, and location of the sensors planned for instrumenting the 1/20-scale central core cavity model. All displacement devices have been functionally tested and bench calibrated.

3.3 PRETEST ANALYSIS

A pretest analysis of the closure plug was undertaken to provide an estimate of the closure's ultimate strength and mode of failure. The estimate of the ultimate strength was intended as an aid in the design of the 1/20-scale model of the closure.

The finite element code ADINA,^{2,3} which contains material models to represent the nonlinear stress-strain response of the steel and concrete components, was used to perform the analysis. However, previous analyses^{4,5} with the concrete constitutive model in ADINA indicated that the model is too stiff under triaxial compressive loadings (such as occur in the closure plug) to sustain a hydrostatic state of stress after compressive failure occurs. Therefore, the concrete constitutive model in ADINA was replaced with a model using the endochronic theory.⁶ The endochronic concrete model is not as stiff as the ADINA concrete model under triaxial compression loadings and includes a strain-softening effect after the peak stress has been reached, which provides a more accurate representation of the concrete behavior.

3.3.1 Analytical Model

A two dimensional (2-D) axisymmetric analytical model was chosen for the analysis. The nonaxisymmetric array of penetrations was modeled by using 2-D axisymmetric elements with reduced stiffnesses to represent the area containing the penetrations. The stiffnesses are reduced by an

Fig. 3.13. Placement of Strain and Displacement Gages for the 1/20-Scale

ent of Strain and Displacement Gages for the 1/20-Scale Model.

Table 3.2. Identification of Function, Type, and Location of Instrumentation on the 1/20-Scale Model of the Central Core Cavity Closure Plug

Gage	Function	Type	Direction of Measurement	Location in Polar Coordinates		
				R (cm)	θ (deg)	Z (cm)
XE-1	Strain	Surface foil	Circumferential	12.70	0	17.02
XE-2	Strain	Surface foil	Axial	12.70	0	17.02
XE-3	Strain	Surface foil	Axial	12.83	0	7.30
XE-4	Strain	Surface foil	Circumferential	12.83	0	7.30
XE-5	Strain	Surface foil	Radial	4.38	270	3.05
XE-6	Strain	Surface foil	Radial	4.50	270	3.05
XE-7	Strain	Surface foil	Circumferential	0.87	90	9.08
XE-8	Strain	Surface foil	Axial	0.89	90	8.77
XE-9	Strain	Surface foil	Circumferential	1.27	330	16.13
XE-10	Strain	Surface foil	Circumferential	1.27	150	16.13
XE-11	Strain	Embedded graphite	Circumferential	1.27	90	10.35
XE-12	Strain	Embedded graphite	Axial	1.27	90	10.35
XE-13	Strain	Embedded graphite	Radial	1.27	90	10.35
XE-14	Strain	Embedded graphite	Circumferential	1.27	270	10.35
XE-15	Strain	Embedded graphite	Axial	1.27	270	10.35
XE-16	Strain	Embedded graphite	Radial	1.27	270	10.35
XE-17	Strain	Embedded graphite	Circumferential	2.54	0	10.35
XE-18	Strain	Embedded graphite	Axial	2.54	0	10.35
XE-19	Strain	Embedded graphite	Radial	2.54	0	10.35
XE-20	Strain	Embedded graphite	Circumferential	2.54	180	10.35
XE-21	Strain	Embedded graphite	Axial	2.54	180	10.35
XE-22	Strain	Embedded graphite	Radial	2.54	180	10.35
XE-23	Strain	Embedded graphite	Circumferential	11.30	175.9	11.54
XE-24	Strain	Embedded graphite	Axial	11.30	175.9	11.54
XE-25	Strain	Embedded graphite	Radial	11.30	175.9	11.54
XE-26	Strain	Embedded graphite	Circumferential	11.30	355.9	11.54
XE-27	Strain	Embedded graphite	Axial	11.30	355.9	11.54
XE-28	Strain	Embedded graphite	Radial	11.30	355.9	11.54
XE-29	Strain	Embedded weldable	Circumferential	1.27	150.0	9.08
XE-30	Strain	Embedded weldable	Circumferential	1.27	330.0	9.08
XE-31	Strain	Embedded weldable	Circumferential	11.30	85.9	11.54
XE-32	Strain	Embedded weldable	Circumferential	11.30	265.9	11.54
ZE-D1	Displacement	Beam transducer	Axial	1.26	10.0	3.05
ZE-D2	Displacement	Beam transducer	Axial	5.18	11.0	3.05
ZE-D3	Displacement	Beam transducer	Axial	10.62	4.7	3.05
ZE-D4	Displacement	Eddy current	Axial	0	0	27.70
ZE-D5	Displacement	Eddy current	Axial	4.37	60.0	27.70
ZE-D6	Displacement	Eddy current	Axial	8.74	60.0	27.70
ZE-D7	Displacement	Capacitance	Radial	9.70	0	27.31
ZE-D8	Displacement	Capacitance	Radial	9.70	0	20.43

amount proportional to the amount of material removed by the penetrations.^{7,8} The actual reduction of the element stiffnesses is accomplished by integrating the stresses in an element over a reduced element volume.⁹

The element mesh layout is shown in Fig. 3.14. Axisymmetric elements were used to represent all aspects of the closure plug. The penetration tubes were modeled by steel elements overlaid on the concrete elements in the penetration region. The resulting smearing of the steel throughout the central portion of the closure prohibits the application of any internal pressure in the tubes. Also no attempt was made to model the steel reinforcement within the closure plug. It was felt that the omission of these two factors would tend to make the analysis conservative.

The resulting analytical representation consisted of 251 nodes, 54 concrete elements, and 54 steel elements. Of the concrete elements, 34 had some stiffness modification, while 40 of the steel elements had some stiffness modification.

Fig. 3.14. Element Mesh Layout for 1/20-Scale Central Core Cavity Closure Model.

3.3.2 Material Properties

The material properties used in the analysis were based on the ASTM specified minimums for the materials used in the prototype. The compressive strength of the concrete used in the analysis was 45 MPa (6500 psi) at 28 d. The steel representing the penetration tubes had a yield stress of 241 MPa (35,000 psi), while all remaining steel components had a yield stress of 414 MPa (60,000 psi).

The use of the minimum values in the analysis was to produce a conservative analysis. It is recognized that the actual material properties will probably be higher, and for this reason an analysis using probable material properties will be undertaken.

3.3.3 Results

The analysis indicates failure at approximately 46 MPa (6700 psi). Failure apparently initiates at the thickened upper portion of the steel liner that encases the closure, and there is additional yielding of the liner at the center of the bottom surface. No significant cracking in the concrete before failure is noted in the analysis.

It should be noted that although the analysis stopped at a pressure of 46 MPa because of considerable yielding of the liner, it is not unreasonable to assume the liner will continue to yield at higher pressure without failing.

3.4 REFERENCES

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4. IRRADIATION PROGRAM

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During 1979 the microfilming of the records of the GCFR irradiation capsules GB-9 and GB-10 was completed. Copies of the microfilms were sent to General Atomic Company (GA) and to Argonne National Laboratory. One copy is stored at the Y-12 document center. The boxes containing the original records were sent to GA.*

*See letter P. R. Kasten to Director, Nuclear Power Development Division, DOE, "GCFR Program: Radiation Capsules GB-9 and GB-10 Completion of Work," May 30, 1979.

GAS-COOLED REACTOR PROGRAMS

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