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Gas-Cooled Reactor Programs

High-Temperature Gas-Cooled Reactor Technology Development Program

Annual Progress Report for Period Ending December 31, 1981

Paul R. Kasten, Program Director
P. L. Rittenhouse, Base Technology Program Manager
D. E. Bartine, Physics and Shielding Program Manager
J. P. Sanders, Component Flow Testing Program Manager

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GAS-COOLED REACTOR PROGRAMS

HIGH-TEMPERATURE GAS-COOLED REACTOR TECHNOLOGY DEVELOPMENT PROGRAM
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Date Published: June 1982

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FOREWORD

During 1981, the High-Temperature Gas-Cooled Reactor (HTGR) National Program continued to emphasize the cogeneration HTGR as the lead candidate for early application. Thus, most of the effort of the HTGR program at Oak Ridge National Laboratory (ORNL) emphasizes technology development for steam-cycle HTGRs. At the same time, the program recognizes the national importance of developing Very High Temperature Gas-Cooled Reactors (VHTRs) for process heat application as the long-term goal and the cogeneration HTGR as a practical stepping-stone toward that goal. Although we anticipate that the ORNL technology development program in the future will gradually shift toward VHTR development, we recognize that much of the steam cycle technology development is also pertinent to VHTRs. At the present time, our VHTR studies have emphasized their application potential to fossil fuel conversion processes. Results to date indicate that a likely first application of VHTRs would be to the steam reforming of methane for chemicals production (e.g., hydrogen or methanol).

Our technology program continues to involve fuels and materials development (including graphite and ceramic structural materials), HTGR chemistry, prestressed concrete reactor vessel (PCRv) studies, reactor physics and shielding studies, component testing of the reactor core support structure, and HTGR application and evaluation studies. Emphasis has been increased on the postirradiation evaluation of fuels by use of the special examination equipment developed at ORNL and on component technology development and testing. In the latter area we have initiated performance testing of ceramic pads (on which the core support posts rest), development of welding techniques for cladding steam generator tubesheets, planning for testing of radiation shields, and application of the component flow test loop (CFTL) to the performance testing of the core support structure, all of which utilize specialized ORNL equipment. Additional emphasis on component technology development and testing is anticipated for the future.

Paul R. Kasten

SUMMARY

1. HTGR CHEMISTRY

An investigation was begun on the transport rate of uranium and plutonium in (and from) grade H451 graphite. Initial tests were concerned with the diffusion rate of uranium (as both UO_2 and UC_2) at 1273 and 1673 K. The procedures adopted are also applicable to future measurements with plutonium to be conducted under alpha laboratory conditions. Preliminary results indicate that the diffusion coefficient of uranium (as UO_2) ranges from about $1.5 \times 10^{-13} \text{ cm}^2/\text{s}$ at 1673 K to about $3 \times 10^{-15} \text{ cm}^2/\text{s}$ at 1273 K.

In recently completed work, measurements were performed on the diffusion of silver in unirradiated H451 graphite. The diffusion profile technique was used for determining diffusion parameters. A limited number of experiments were also performed on the desorption of silver from H451 graphite. We found a surface layer of about 1-mm in depth, in which the adsorbed silver diffuses extremely slowly. Beneath this surface layer, the diffusion of silver is rapid compared with other materials. An expression for the diffusion coefficient as a function of temperature is presented for this fast path mode.

A study of the sorptive capacity of H451 and Stackpole 2020 graphite for iodine was completed. Test temperatures ranged from 250 to 1000°C; iodine partial pressures ranged from 1 to about 10^{-6} Pa (10^{-5} to $\pm 10^{-11} \text{ bar}$) in purified helium. Sorption isotherms for this test parameter range are presented.

2. FUELED GRAPHITE DEVELOPMENT

The Fueled Graphite Development Program at ORNL is organized into four subtasks: (1) Irradiation Testing, (2) Development Work, (3) Project Work Statement (PWS) Activities, and (4) Equipment Development and Maintenance. Highlights covering work in these areas during 1981 are given below.

Irradiation Testing

- The capsule construction and operation work is no longer included under the Fueled Graphite Development Program per se; rather, these tasks are now included in the General Atomic Company (GA) Irradiation Services (FTP/A 01337) portion of the ORNL Gas-Cooled Reactor Program and are reported in Chap. 3. As a result, the irradiation testing subtask of the Fueled Graphite Development Program now includes only postirradiation examination (PIE) work

and performance assessment activities. Because the major portion of the ORNL-Kernforschungsanlage (KFA) Jülich cooperation in the High-Temperature Gas-Cooled Reactor (HTGR) fuel development area lies in PIE and performance assessment, this subtask and subtask 3 overlap somewhat.

- During this reporting period, PIE was accomplished on capsules HT-35, HRB-14, and HRB-15b. Examinations were completed on HRB-14 and -15b. Irradiated microsphere gamma analyzer (IMGA) and post-irradiation gas analyzer (PGA) examinations were deferred for the HT-35 fuel to accommodate GA's desire to have the PIE on HRB-15a fuel completed first.

Development Work

- No additional work was done during this reporting period on pyrocarbon coatings fabricated with CO₂ dilution of the coating gas. A report on the fabrication and characterization of these coatings was written. We concluded that pyrocarbon coatings deposited with CO₂ as a diluent were more gastight (as-produced) than those deposited with helium, argon, or H₂ dilution. The final evaluation of these coatings must await completion of the evaluation of the particles irradiated in capsule HT-35. As indicated earlier, the HT-35 examination has been postponed until the HRB-15a PIE is completed.
- A number of batches of SiC coatings have been studied and characterized during the year. Methods of examination included x-ray diffraction, transmission electron microscopy (TEM), Raman spectroscopy, and small-angle x-ray scattering (SAXS). In addition to coatings produced at ORNL, several German batches and two from GA were examined. The methods of characterization appear to correlate reasonably well for the various SiC coatings, but fission product retention cannot yet be related directly to the SiC properties and characterization.
- Work on development of fabrication techniques for "dense UCO" kernels continued. Effort during this reporting period concentrated on scaling up the laboratory operation to a batch size compatible with prototype operation by GA.

Project Work Statement Activities

- Characterization studies with x-ray diffraction, TEM, scanning electron microscopy (SEM), Raman spectroscopy, and SAXS were completed on three batches of German SiC coatings. This work was accomplished under PWS FD-10.
- Data analysis was completed and a final report written on the work done under PWS FD-12 (permeability studies on Biso-coated particles).

- Data analysis was completed and a final report written on the work done under PWS FD-13 [comparison of IMGA and postirradiation annealing and autoradiography (PIAA)].

Equipment Development and Maintenance

- Irradiated microsphere gamma analyzer
 1. A design problem with the particle singularizer was corrected.
 2. Detector and computer problems were corrected.
 3. Downtime for maintenance during the year was about 40%.
 4. A new input/output terminal was added to the system.
 5. A new backup data storage device was also added. This device contains both a fixed disk drive unit and a removable floppy disk drive unit. This equipment replaces the original Data-Pacer device.
 6. Discrepancies in the cross section sets used to calculate fuel burnups and operating temperatures were resolved by use of IMGA-derived fission product inventories and activity ratios.
- Postirradiation gas analyzer
 1. The high-temperature particle-breaking station was made operational.
 2. The data analysis procedure was improved to increase resolution and reproducibility of the data.
 3. New calculational procedures were developed to couple IMGA and PGA data.
 4. A new turbomolecular pump was procured to replace the existing ion pump. Specifications were developed for the purchase of a quadrupole mass spectrometer for use with the high-temperature breaking station.
- X-ray micrographic facility
 1. System optimization was completed (long exposure times cause fogging of the film from radioactive samples, but short exposure times reduce resolution).
 2. The facility was used in the PIE of fuel from HRB-14 and HRB-15b.
 3. IRRADIATION SERVICES FOR GENERAL ATOMIC COMPANY

This work is identified as a separate chapter for the first time. Fuel irradiations were reported on in Chap. 2, Fueled Graphite Development Program, before 1981. Accomplishments during 1981 include

- Completion of the irradiation of capsule HRB-15a. This capsule operated for 4172 h to a peak fast neutron fluence of 6.9×10^{25} neutrons/m² ($E > 29$ fJ)_{HTGR}. The end-of-life fission gas release-to-birth rate ratio (R/B) for ^{85m}Kr was 3.8×10^{-4} . The peak burnups were 85% fissions per initial (heavy) metal atom (FIMA) for ²³⁵U, 16% FIMA for ²³⁸U, and 6.4% FIMA for ²³²Th. This capsule will be examined at ORNL under the Fueled Graphite Development Program.
- Design, fabrication, and assembly of capsule HRB-16. The fuel for this experiment was manufactured by GA. The capsule design was similar to previous HRB capsules, except that a new thermocouple design was used in one location. This design (called TCAT for thermocouple array tube) permits thermocouple junctions at several axial locations (four locations in this experiment) while using only one thermocouple hole in the graphite sleeve.
- Completion of irradiation of capsule HRB-16. The capsule operated for 4082 h to a peak fast neutron fluence of 6.7×10^{25} . The maximum fission gas R/B was 2.1×10^{-4} for ^{85m}Kr. Peak burnups were 84, 14, and 6.7%, respectively, for ²³⁵U, ²³⁸U, and ²³²Th.

4. PRESTRESSED CONCRETE PRESSURE VESSEL DEVELOPMENT

The Prestressed Concrete Reactor Vessel (PCRV) research and development program consists of generic studies to provide technical support for ongoing PCRV activities, to contribute to the technological data base, and to provide independent review and evaluation of relevant technology. During this reporting period the work was organized into six subtask areas: (1) analysis methods development; (2) concrete properties; (3) tendon and reinforcing steel materials; (4) instrumentation evaluation and development; (5) structural model tests; and (6) liner, cooling, and insulation systems. Highlights of work under each of these subtask areas are

Analysis Methods Development

- Completed a set of computer subroutines for finite-element computation of creep effects in concrete structures. The program uses a rate-type creep law based on expanding the relaxation function in Dirichlet series. Concrete aging is accounted for, and temperature effects on the creep rate and the rate of aging are considered. Material properties are entered, either as empirical creep data or as double-power law for creep.
- Developed a technique for embedded-rebar modeling. Through the use of positional nodes and an additional basis coordinate, arbitrarily oriented rebars can be embedded in the isoparametric continuum elements. The technique facilitates the modeling of complex arrays of rebars, such as those around penetrations in PCRVs.

- Developed a computer program for calculating equivalent nodal loads resulting from distributed loads on a two-dimensional finite-element model. The program eliminates the need for a user to convert pressure loads to equivalent nodal loads before performing an analysis.

Concrete Properties

- Completed a review on concrete properties in a nuclear environment. Information contained in that review is described as follows: Concrete material systems were reviewed with respect to constituents, mix design, placing, curing, and strength evaluations. Typical concrete property data and information on specialty concretes were reviewed.
- Developed a performance specification for a concrete elevated-temperature multiaxial material testing system. Covered in the specification is information on: general requirements, load frame assembly, an environmental system, specimen development and actuator stroke instrumentation, an interactive computer control and data acquisition system, and functional requirements of computer programs for system control data acquisition and analysis. When fabricated, the system will provide data representative of PCRV concrete behavior under normal and postulated accident conditions.

Tendon and Reinforcing Steel Materials

- Completed a review on steel reinforcement and prestressing for application to PCRVs. Information contained in the review is described as follows: Steel reinforcement and prestressing systems were reviewed with respect to their function in PCRVs, material properties, fabrication procedures, and code requirements.

Instrumentation Evaluation and Development

- Completed an evaluation of nondestructive testing techniques (including acoustic emission) for application to PCRVs. Information contained in the evaluation is described as follows: Nondestructive testing techniques were described and their applicability to PCRVs were assessed. Potential applications of acoustic emission to PCRVs as well as limitations of the technique were identified. A summary of acoustic emission applications to concrete material systems and structures was presented.

Structural Model Tests

- Developed techniques for the wire winding of PCRV models and model liner systems. A vendor has been identified, who has a wire-winding facility that can apply prestressing to the PCRV models in accordance with a specification that has been developed. Fabrication and leak testing of the 12-gage AISI 1008 drawing-quality steel liners for the PCRV models were completed. All ancillary equipment required for conduction of the model tests was designed and fabricated.

- Completed a test plan for conduction of structural tests of thermal barrier ceramic pads in accordance with a test specification developed by GA. Methods for instrumenting, loading, and monitoring the ceramic pads were developed.

Liner, Cooling, and Insulation Systems

- Completed a review on liner, cooling, and insulation systems for PCRVs. These systems were reviewed with respect to their functions, code requirements, and experimental tests conducted in support of their design. Systems that have been used or proposed for PCRVs were reviewed.

5. HTGR STRUCTURAL MATERIALS

Design data for various components of HTGR systems are being provided through structural materials efforts at ORNL. The ORNL materials program is divided into six major task areas as described below.

Mechanical Properties

A number of long-term creep tests on Hastelloy X and 2.25 Cr-1 Mo steel have now exceeded 40,000 h in HTGR-helium. The current major test emphasis is on these alloys, on various weldments of Hastelloy X, and on Inconel 617 and its modifications. To date, carburization has had no detectable effect on creep rate, but fracture strain is often reduced.

Aging exposures of alloys in HTGR-helium have reached more than 30,000 h, and postexposure testing of materials aged for 20,000 h is either complete or in progress. Both aging and interaction with the environment tend, depending on aging time and temperature, to reduce fracture resistance and strain at fracture.

Investigation of the effects of aging and test conditions on the low-cycle fatigue behavior of Hastelloy X and Inconel 617 has been completed. Testing in an HTGR-helium environment was in no case detrimental to fatigue life and was usually beneficial for Inconel 617. The effect of aging was more significant and, depending on exact aging conditions, sometimes increased and sometimes decreased fatigue life. Ongoing efforts are focused on the creep-fatigue and high-cycle fatigue of Hastelloy X.

High-precision compressive creep tests have been run on Corning 7941 fused silica; large sample-to-sample variation in creep response has been observed. Recent testing, both compressive and bending creep, has concentrated on silica fabricated by Thermo Materials. Considerable effort was expended to qualify equipment for the stress-relaxation testing of ceramics.

Corrosion

Studies of carburization-dilation effects in Hastelloy X and alloy 800H were completed from 750 to 1000°C. Maximum dilations of 0.6%

were observed. The relationship between dilation and carburization was almost linear up to 100 g/m^2 . Beyond this point dilation stabilized.

Depletion and generation of impurities in HTGR-helium test environments by gas phase and gas-metal reactions were investigated. Measurements made over a range of flow rates and water vapor contents showed that the extent of depletion-generation varies with both gas flow rate and water vapor level. Further, it was shown that the degree of carburization of various alloys is strongly dependent on water and methane supply rates, where supply rate is expressed as a mass of reactant per unit of area per unit time. These observations help to explain ambiguous carburization data generated by different investigators.

Joining Technology

The joining technology task has investigated the fusion weldability of cast nickel-base superalloys and a mechanically alloyed oxide dispersion-strengthened (ODS) alloy. The laser and electron beam welding processes were used to join the cast alloy 713LC and the ODS material MA956. Creep test results were obtained for weldments in each of these materials. A change of emphasis in the HTGR program has prompted the initiation of a study on weld cladding of alloy 800H tubesheet forgings.

Structural Ceramics

The resilience of three fibrous insulations has been evaluated after aging in HTGR-helium for periods of up to one year at 816°C . The data taken indicate that aging for periods of longer than three months may degrade the resilience of these materials, but a final conclusion awaits the results of measurements on specimens being aged for two years.

Depressurization testing of structural and fibrous ceramics was completed. Little or no effect of the rapid pressure transients was seen except for Masrock fused silica. Three out of four specimens of this material disintegrated during the depressurization from 7.9 MPa.

Carbon fiber composites were investigated as possible candidates for core support pads in advanced HTGR systems. Although results have been encouraging, this work was terminated to permit greater effort on the alumina and silica reference materials.

Fine-grained Thermo Materials fused silica and silicon nitride-type NCX-34 have been characterized both as-received and after creep testing in HTGR-helium. Characterization methods included both scanning and transmission electron microscopy.

Fission Product-Materials Interactions

Samples of Hastelloy X and Inconel 617 are being exposed in a helium carrier gas of buffered oxygen content containing traces of both cesium and tellurium vapor. A number of modifications to the test equipment and

to test procedures have been required to minimize problems with deposition of these elements at positions upstream from the samples. Intergranular cracking has occurred in early tests with tellurium vapor only.

Liner and Penetration Studies

The fracture toughness of weldments of various heavy sections of steel expected to be used in PCRV penetrations was examined. The testing program included Charpy V-notch and drop-weight impact, subsize and standard tensile, and compact specimens, as well as instrumented pre-cracked Charpy specimens for static and dynamic fracture toughness. Particular attention is being given to lower bound toughness in the analysis of the data.

6. HTGR GRAPHITE STUDIES

Graphite Creep

Capsule OC-4 (600°C) was removed from the reactor, and capsule OC-5 (900°C) was inserted. Results from OC-4 are now available. There can no longer be any question that the secondary creep rate is decreasing with fluence, suggesting a potential problem in stress buildup as the graphite approaches end of life. It now is also well established that primary creep leads to a significant and recoverable volume change, whereas secondary creep proceeds at constant volume. The thermal expansion remains the only material property being followed that is affected directly by the creep.

Irradiation of German Reflector Graphites

Irradiation of candidate graphites for the Federal Republic of Germany continues. Roughly speaking, the graphites are comparable to the U.S.-made core graphite H451 in irradiation behavior. A disturbing factor is the varying behavior of different lots of presumably identical grades of material, which indicates that some minor modifications in manufacture have led to profound microstructural changes.

Experiments continue with HTK-5, now in the reactor. Currently both U.S. and German graphites are being included. The longest lived graphite yet observed is GraphNOL N3M, an aerospace material developed at ORNL for extremely high strain-to-failure. Originally, the ORNL fabrication program was initiated to develop a radiation-resistant graphite but was transformed into an aerospace program for the U.S. Department of Defense (DOD). In the end it has apparently attained its original objective.

Graphite Oxidation

Oxidation studies continue, with samples in statistically significant numbers being produced. The Weibull m -statistic continues to be observed as unchanging with oxidation. The major addition to the program this year has been the addition of surface areas, porosimetry, and helium

densities to the list of routine property measurements. These microstructural properties show effects as might be expected: large but saturating increases in total surface area, pronounced increase in accessible void volume at the expense of inaccessible void volume, and a marked increase in effective pore diameters in the intermediate range of diameters.

Two other measures are yet targeted for oxidation studies: gas porosimetry and active-site generation. The gas porosimetry is currently being developed. In the end the complete set of microstructurally sensitive measurements should allow the material properties to be introduced into the oxidation rate equation.

Graphite Characterization

A preliminary statistical analysis of tensile strength measurements on H451 graphite has been completed. The analysis is apparently successful in dichotomizing the strength into a normal distribution and a disparate low-strength distribution resulting from large flaws introduced during fabrication. A one-way variance analysis was also successful. Because the analysis is general and the bimodality of the distribution is of considerable design significance, this preliminary analysis justifies a much more detailed and sophisticated study.

7. HIGH-TEMPERATURE REACTOR PHYSICS STUDIES

Work performed this past year has been directed toward improvement of finite-difference diffusion-theory calculations with emphasis on improved accuracy for some applications and reduced computational time for others. Implementation of a mesh-edged finite-difference code and a fuel management (with economics) module was completed for use with the local neutronics system.

Analysis of the HTGR was concentrated in those areas that looked promising for a better understanding of the fuel cycle performance but without the expense of full-core, three-dimensional calculations. A three-dimensional model of the fundamental unit cell made up of a partial fuel element from each refueling batch was used to study power density variation within a region containing fuel elements with quite different burnups. Schemes to correlate flux variation and boron cross section with burnup were investigated. A perturbation scheme capable of predicting changes in reactor conditions with fuel depletion shows some promise after preliminary analysis. Coupling of HTGR thermal-hydraulic capability to VENTURE was initiated.

Calculations were performed to assess control rod worth in a 1640-MW(t) Pebble Bed Reactor (PBR). Three-dimensional Monte Carlo calculations were performed to estimate the reactivity effectiveness of control rods when they were moved from the top reflector region to the gaseous region immediately above the core; a reactivity worth of about 3.3% was calculated. Results obtained with discrete-ordinate calculations compared favorably with the Monte Carlo results.

8. HIGH-TEMPERATURE REACTOR SHIELDING STUDIES

Work was directed toward the design of the HTGR bottom reflector and support block neutron streaming experiment to test methods and data and to verify radiation levels in the HTGR lower regions. The work started with a survey analysis of radiation levels in the HTGR lower regions. We found that the maximum thermal neutron fluence in the sidewall thermal barrier cover plate was influenced greatly by neutron streaming through the large coolant holes in the reflector and support blocks and that it exceeded the design constraint of $10^{21}/\text{m}^2$ by a factor of 10.5. Other radiation levels appeared to be well below the corresponding design constraints.

The design of the experiment was developed with data from the design analysis, and the preanalysis was begun. The Tower Shielding Reactor source calculation was completed, and modeling the geometry for the first boron pin layer began.

9. HIGH-TEMPERATURE REACTOR COMPONENT FLOW TEST LOOP STUDIES

The Component Flow Test Loop (CFTL^{*}) was constructed to a phase of completion designated Stage Alpha under the Gas-Cooled Fast Reactor (GCFR) Program during FY 1981. With the closeout of that program, the loop was dedicated to problems related to the design data needs for the baseline program aimed at the development, design, and construction of a demonstration HTGR plant. In the Stage Alpha, the test vessel was replaced with a length of pipe to close the loop, only 1 of 13 planned 0.5-MW controllers was installed on the heater power supply, the electrical heater and its associated wiring were not installed, and loop instrumentation was not connected to the data acquisition system.

Funds were supplied from the High-Temperature Gas-Cooled Reactor (HTGR) Program to study the application of the CFTL to the HTGR program and to make modifications in the final stages of construction to facilitate this use of the loop. To enhance the applicability of the loop for the HTGR, the piping was modified to provide upflow at the test vessel and to extend the operational verification test of the gas-bearing circulators at the manufacturer's plant to demonstrate their ability to operate with gas temperatures of 450°C (850°F), which is greater than the original specification of 340°C (650°F).

The study of applications for the CFTL indicated the importance from the standpoint of both safety and plant availability of investigating the performance of the core support structure for the prismatic core of the HTGR. This structure includes vertical graphite posts that support the lower graphite blocks of the core. These posts are installed at the time the core is initially assembled, and they must function for the planned lifetime of the core. The posts are located in the exit gas stream, and, therefore, they are exposed to the maximum gas temperatures. At these temperatures, they are susceptible to reaction with low levels of

^{*}Before May 1981, CFTL represented "Core Flow Test Loop."

moisture or other oxidants in the circulating helium. In addition, the end of the post has a spherical surface that mates with a seat having a spherical surface with a larger radius. The limited area of contact produces stress concentrations (called Hertzian stresses) in both the seat and the post. The interrelated effect of this stress and possible chemical reaction of the graphite with gaseous impurities must be investigated for the actual geometries of the core support structure.

The core support performance test (CSPT) has been proposed, and the design of the test vessel and required loop modifications have begun. To the extent possible, the attributes of the existing completed Stage Alpha loop have been incorporated into the design. Parts that were ordered and received for the original CFTL design but not installed in Stage Alpha have been used in the design of the test vessel and necessary loop modifications. An impurity measurement and control system was designed for the CFTL but not purchased. This design has been modified to match the impurity levels required for the CSPT.

An analysis has been made to determine the impurity levels required in the CSPT to permit a six-month test to represent the nature and character of the chemical-structural interaction during 40 years of operation in the reactor. Special consideration has been given to the choice of structural materials in the test vessel to prevent adding contaminants that will inhibit or catalyze the reaction of the graphite with the gaseous impurities. We have begun small-scale tests to verify this contingency and to test the compatibility of materials specified for a heater that will be used to heat the helium to the specified temperature at the test piece.

All design work for the CSPT had begun by the last quarter of 1981. The design of the test vessel was complete, and the designs of the vessel internals and the piping modifications were nearing completion. Specifications for the impurity-measuring system were prepared and subjected to initial review by the project.

Several procurement activities for the CSPT were started; particularly, purchase orders were placed for additional control equipment to provide a total of 1.5 MW of controlled power to the helium heater.

The final construction activities for the Stage Alpha CFTL were completed by the end of September 1981. The subsequent shakedown activities, in particular, the operation of the three gas-bearing helium circulators in series at design speed, were completed by December 1981. The as-built revisions of drawings and documentation for the mechanical and the instrumentation and controls disciplines were completed and approved.

10. APPLICATION AND PROJECT ASSESSMENTS

Potential HTGR applications to process heat and to fossil conversion processes were summarized. Estimates were made of the capital costs of drilling for natural gas versus those for producing substitute natural

gas (SNG) from coal via either all-coal or HTGR-coal hybrid processes. The conclusion for the near term at least is that the capital costs of drilling for gas are much lower than for converting coal to SNG.

Differential Economic Estimates of HTGR Application to Fossil Fuel Conversion Processes

Differential economic estimates were made to determine the most attractive HTGR applications to fossil fuel conversion processes; the criterion of merit was the present value of avoided feedstock costs. Both very high temperature reactors (VHTRs) (generally associated with reformer HTGRs) and steam cycle-cogeneration (SC-C) HTGRs were considered.

For the VHTR, steam reforming of methane for chemical production had the highest permissible HTGR investment, with a present value of permissible investment of \$2.0 million/MW(t); this application also requires little process development. The Solvent Refined Coal-II (SRC-II) process (with upgraded fuels) permitted a relative VHTR investment of \$1.7 million/MW(t); the TOSCO-II process permitted an investment value of \$1.2 million/MW(t).

For the HTGR SC-C, the H-Coal (standard) process permitted a relative HTGR investment of about \$1.3 million/MW(t) compared with \$580 thousand/MW(t) for the Exxon Catalytic Coal Gasification (ECCG) process. There was no economic incentive to apply VHTRs rather than HTGR SC-Cs to the ECCG process.

HTGR Reformer Studies and Process Heat Transport Modeling Studies

Review of Thermochemical Pipeline System Costs. Earlier cost estimates by ORNL and the General Electric Company (GE) on thermochemical pipelines (TCPs) for energy transmission were evaluated. A number of differences in economic factors were identified, the largest being in the piping and transportation systems. Some important specific differences were (1) a 30% contingency factor was added to the piping cost by ORNL, but GE used zero contingency; (2) off-site pumping stations were added by ORNL whenever the fluid pressure dropped by 1/10th its initial pressure, but this was not included by GE; (3) a maximum pipe diameter of 122 cm (48 in.) was considered by ORNL versus a 138-cm (54-in.) diameter by GE [122-cm-diam (48-in.) pipe is the largest size commercially available in the United States today]; and (4) the ORNL unit compressor cost estimate was about 30% higher than that of GE. This evaluation concurs with the earlier ORNL cost estimates by Stovall et al. and concludes that the GE estimates are too low.

Process Heat Reactor Dynamics Studies. This work provides independent evaluations of dynamics, control, and safety problems with advanced-concept HTGR plant designs. Operational simulations of most of the major components of a reformer HTGR (R-HTGR) plant have been completed, including the reactor core, intermediate heat exchanger, steam generator, reformer (in part), turbine generator, and pipeline. Completion of the reformer simulation is awaiting resolution of constants to be used in the

methanation rate equation. Plant simulation of the sensible energy transport and storage (SETS) plant was completed, including development of load-following control schemes. The SETS plant simulation showed the reference plant to have excellent inherent and controlled dynamic characteristics for the primary, secondary, and salt loop systems.

Outlet Gas Temperatures for Advanced HTGRs

An evaluation of the appropriate outlet gas temperature for various HTGR advanced applications indicated that it is still prudent to retain 950°C as the maximum reactor-outlet temperature as the program objective.

Review of General Atomic Company HTGR Safety Studies

A detailed review of previous HTGR safety studies performed by GA indicates that there is difficulty in specifying the probabilistic distribution of risks because of the lack of pertinent data. Although probabilistic risk analysis can provide useful insight and assist in design decisions, substantial amounts of pertinent operating data on system and component behavior are still needed. This need is emphasized by the need to validate accident analysis models. Attention should be given to validating both the deterministic simulation models and the component and system reliability data base.

Modular HTR Studies

Information on small modular HTR designs is being collected for use in evaluation studies of prismatic-(HTGR) and pebble-bed-fueled concepts. Preliminary core designs were identified for physics evaluation studies.

1. HTGR CHEMISTRY

R. P. Wichner

1.1 URANIUM AND PLUTONIUM DIFFUSION IN GRAPHITE — O. K. Tallent and R. L. Towns

In an investigation initiated to study the diffusion properties of uranium and plutonium in H451 graphite, uranium diffusion tests were conducted at 1000 and 1500°C. The equipment and procedures used are expected to be applicable to the study of plutonium diffusion at a later time.

A method was developed to adsorb uranium (and plutonium) in a uniform layer on the surface of 0.80-cm-diam by 1.25-cm-long H451 graphite pellets. The pellets were soaked in ethanol containing 0.01 to 0.10 M $\text{UO}_2(\text{NO}_3)_2$ and subjected to a drying and calcination procedure that converted the adsorbed uranyl nitrate to dioxide. Scanning electron micrographs and x-ray maps showed the adsorbed uranium to be widely and evenly distributed on the pellet surfaces. The amounts of uranium adsorbed can be defined with a correlation coefficient of 1.0 by the equation:

$$U_{\text{ads}} = 9.87 U_{\text{sol}} + 0.01 ,$$

where U_{ads} denotes the amount of adsorbed uranium in milligrams per square centimeter of geometric pellet surface area and U_{sol} denotes the molar concentration of uranium in the ethanol solution.

For the diffusion tests, the pellets with adsorbed uranium were designated as "supplier pellets." These supplier pellets were heated in contact with pellets initially free of uranium, designated as "receiver pellets," in specially designed graphite holders for one-, two-, and three-week periods under a stream of highly purified helium gas. Tests at

1000°C were conducted in quartz containers; tests at 1400°C, in alumina containers. At the end of the heating periods, the pellets were analyzed for uranium by neutron activation analysis, and the pellet surfaces that were in contact during the heating periods were ground off to depths of 25 to 60 μm (Table 1.1). The mean uranium concentration in megagrams per cubic meter in the ground-off surface material of the supplier pellets and the total uranium remaining in the receiver pellets after the grindings were also determined by neutron activation analysis. Employing the values listed in Table 1.1, the following expression was used to determine the average diffusion coefficient $\bar{D}$ during the experiment. Implied in this approximation is the assumption of a linear concentration profile with time-invarying slope over the duration of the experiment:

$$\bar{D} = h\Delta C_R / A\bar{C}_s t ,$$

where h , ΔC_R , A , $\bar{C}_s$, and t , respectively, denote the graphite depth ground from the receiver pellets, the total mass of uranium remaining in the receiver pellets after grinding, the contact surface area between the supplier and receiver pellets, the mean uranium concentration, and the heating time in seconds. The $\bar{D}$ values obtained from one-, two-, and three-week diffusion tests at 1000°C varied as shown in Table 1.1 from 2.1×10^{19} to $8.5 \times 10^{19} \text{ m}^2/\text{s}$. For two tests at 1400°C, the values were 1.1×10^{17} and $2.6 \times 10^{17} \text{ m}^2/\text{s}$. Because it is difficult to prevent errors in diffusion experiments, we believe that both analytical and experimental errors contributed to the variation. The diffusion coefficients are plotted as a function of reciprocal temperature in Fig. 1.1. The Arrhenius equation

$$\bar{D} = 5 \times 10^{-8} \exp(-41,500/1.97T)$$

defines the coefficients with a 0.93 correlation coefficient. In this equation, the data are for two temperatures only, $T = 1273$ and 1673 K , and Arrhenius plots of diffusion data frequently prove to be nonlinear. Use of the equation should be limited to the calculation of approximate values of $\bar{D}$ from 900 to 1500°C.

Table 1.1. Mean diffusion coefficients for diffusion of uranium present as UO_2 in H451 graphite

Time (d)	Temperature (K)	Supplier pellet mean U concentration ^a (Mg/m^3)	Receiver pellet total U ^b (μg)	Graphite depth ground from receiver pellet (μm)	Mean diffusion coefficient (m^2/s)
7	1273	0.152	0.106	25	5.8×10^{19}
7	1273	0.152	0.156	25	8.5×10^{19}
14	1273	0.152	0.091	33	3.3×10^{19}
14	1273	0.152	0.112	29	3.5×10^{19}
21	1273	0.102	0.096	25	2.6×10^{19}
21	1273	0.108	0.104	25	2.7×10^{19}
21	1273	0.174	0.132	25	2.1×10^{19}
7	1673	0.034	0.420	60	2.6×10^{17}
7	1673	0.061	0.840	25	1.1×10^{17}

^aThis is the mean uranium concentration (as UO_2) in the first 25 μm of graphite depth from the surface, $\pm 10\%$.

^bThis is the uranium remaining after grinding off the surface in contact with the supplier pellet, $\pm 10\%$.

Fig. 1.1. Mean diffusion coefficient for uranium present as UO_2 in H451 graphite.

The diffusion of uranium present as uranium carbide, UC_2 , was also investigated. Completion of this investigation was delayed by a temporary shutdown of a TP-5000 gamma-ray spectrometer used to determine the extent of uranium penetration of samples. On the basis of total uranium transfer data, the diffusion coefficients for uranium present as UC_2 are about an order of magnitude greater than the coefficients for uranium present as UO_2 . A topical report on this work is in preparation.¹

Work to assemble equipment and to clean up alpha glove boxes for use in measuring the diffusion rate and vapor pressure of plutonium adsorbed on graphite is in progress.

1.2 SILVER TRANSPORT IN H451 GRAPHITE — R. A. Causey

In recent completed work,² measurements were performed on the diffusion of silver in unirradiated H451 graphite. The diffusion profile technique was used for determining diffusion parameters. A limited number of experiments were also performed on the desorption of silver from H451 graphite.

Diffusion profiles for silver in H451 graphite were measured from 490 to 800°C. Maximum silver concentrations were varied from 10 ng/g to 1 µg/g. In early experiments, the diffusion along and across the axis of extrusion was compared, but no difference was seen. All subsequent experiments were performed with the diffusion direction across the axis. Typical results for the diffusion profiles are shown in Fig. 1.2 for diffusion into solid cylinders of 0.64- and 1.27-cm diameter.

Two features are apparent for all the diffusion profiles: a high-concentration, slow or nonmoving component occurs near the surface, and a low-concentration, faster moving component occurs away from the surface. This behavior is similar to results for cesium diffusion in graphite.

The silver component near the surface appears to be trapped on active sites created by the sample machining. These active sites are probably a combination of broken crystallites and machining powder left on or near the surface. A decrease in the fraction of the silver appearing in the surface component was seen when the sample surface was cleaned, but it was not possible to lower the fraction to zero.

Fig. 1.2. Silver diffusion into solid graphite cylinders.

An Arrhenius plot of the "fast path" diffusion coefficients is shown in Fig. 1.3. The solid line is the least-squares fit to the data and is represented by $D_0 = 17.4 \text{ m}^2/\text{s}$ and $Q = 183.6 \text{ kJ/mol}$ in the expression $D = D_0 \exp[-Q/RT]$. The scatter in the data is typical of that in diffusion experiments.

Figure 1.4 compares our data with that reported by other authors for silver in graphite and pyrolytic carbon. The measured diffusion coefficients for silver in H451 graphite are considerably higher than those reported for silver in pyrolytic carbon and irradiated graphite. We conclude that the difference between the results for H451 and pyrolytic carbon is due to the difference in the amount of porosity of each material.

Fig. 1.3. Arrhenius plot for the "fast path" diffusion coefficients.

Fig. 1.4. Diffusion coefficients for silver in various graphites.

1.3 ADSORPTION-DESORPTION CHARACTERISTICS OF IODINE ON GRAPHITE — R. A. Lorenz

The adsorption-desorption behavior of elemental iodine vapor on types H451 and S-2020 graphite compacts was investigated to provide information about the distribution of iodine in the primary circuit of a high-temperature graphite-moderated reactor. The tests were performed at atmospheric pressure in helium containing ^{131}I -traced iodine arranged to flow continuously through the open pores of a 6.6-g cup-shaped graphite specimen. The iodine partial pressure of 5×10^{-6} to 10^{-1} Pa (5×10^{-11} to 10^{-6} bar) was attained by passing part of the helium flow through a U-tube containing elemental iodine refrigerated from -29 to -69°C . Two NaI radiation counters monitored both the amount of iodine on the graphite and the cumulative amount passed through or desorbed from the graphite. Most previous testing by others was conducted in vacuum.³⁻⁷

The results of our tests with type H451 graphite are shown in Fig. 1.5. Six different test pieces cut from the same block of graphite were used, and the data include points for readsorption following desorption.

Fig. 1.5. Equilibrium sorption of iodine on H451 graphite.

The results of our work and others³⁻⁷ at 400°C are shown in Fig. 1.6; the iodine loading is in micrograms of iodine per square meter of BET surface area. The reason for the large differences in adsorption is not understood; it may be a combination of test atmosphere (vacuum versus helium), surface exposure (powder versus compact), or graphite properties not yet evaluated.

Fig. 1.6. Adsorption of iodine on graphite at 400°C.

The effect of temperature at 1.25×10^{-4} Pa is shown in Fig. 1.7. The peak in adsorption at 750°C with the S-2020 graphite may result from impurities because the impurity level in S-2020 (≈ 500 ppm) is about 10 times that of H451 (≈ 50 ppm). Iwamoto and Oishi⁶ found a similar peak with powdered natural graphite, which probably was also relatively impure.

Isothermal desorption tests were performed by forcing purified helium through the test specimen after an equilibrium loading was attained. When the logarithm of the loading was plotted against the square root of time, straight-line behavior was usually observed. Fifty percent desorption

Fig. 1.7. Adsorption of iodine at 1.25×10^{-4} Pa (1.25×10^{-9} bar).

occurred in about 10 min for H451 at 600°C . Desorption from the 750°C peak of S-2020 was extremely sensitive to small pressure and flow rate changes. Iwamoto and Oishi⁶ also observed desorption peculiarities at 750°C with natural graphite; desorption was slow in vacuum but increased rapidly when argon was added.

Ramp desorption, beginning at 250°C and increasing $3^{\circ}\text{C}/\text{min}$, was typically 50% complete by about 475°C and was essentially 100% complete by 900°C . Previously measured equilibrium loadings were then duplicated without hysteresis, indicating reversible adsorption behavior.

Our tests conducted with low concentrations of iodine in flowing helium revealed significantly lower adsorption than previously measured for other types of graphite tested in vacuum. Commercial high-temperature gas-cooled reactors (HTGRs) are expected to operate with iodine partial pressures from 10^{-7} to 10^{-4} Pa (10^{-12} to 10^{-9} bar).⁸ The actual iodine pressure will depend on the rate of release of iodine from the fuel, its adsorption on graphite and on steel heat exchanger surfaces, and on the

rate of removal by the helium purification system. Our data provide more realistic information needed for these design considerations as well as for the prediction of iodine behavior during accidents. A final report is being prepared.⁹

1.4 IODINE SORPTION ON STEEL — J. I. Brand and R. B. Briggs*

The primary purpose of this year's work was to compare the steady-state concentrations of iodine on type T-22 steel at temperatures below 400°C with those at 400°C for a range of iodine burdens in helium. Secondary purposes were to test a type and arrangement of adsorption specimens that differed from those used in previous experiments and to determine the effect of moisture on iodine adsorption.

The adsorption of iodine on two 5- by 10- by 0.5-mm type T-22 steel plates was measured in tests at 400, 300, and 200°C. In these tests the specimens were exposed to helium that contained an iodine burden of from 3×10^{-4} to 3×10^{-6} Pa. Desorption of iodine was followed in two tests. In one at 300°C, the iodine burden was reduced to 3×10^{-6} and subsequently to 0 over specimens in equilibrium with an iodine burden of about 3×10^{-4} in dry helium. In the other test, the iodine burden was reduced from 3×10^{-6} Pa to 0, and the temperature of the specimens was raised stepwise from 200 to 500°C. Some effects of exposing the specimens at 400°C to moist helium were observed in a test at the end of the experiment.

The amounts of iodine adsorbed on the specimens were inferred from measurements of the radiation from ^{131}I used as a tracer. The specimens were positioned by means of a stainless steel wire holder. Iodine adsorbed on the wire contributed to the activity measured for the specimens. Although we concluded that about 80% of the activity was attributable to iodine on the specimens, the amount might have been as little as 60%. Amounts of iodine present on the specimens were calculated on the basis that all the activity was contributed by iodine on the specimens. The amounts, so reported, are likely to be too large by a factor of 1.2 to 1.7.

*Consultant.

In phase 1 of the experiment with the specimens at 400°C, steady-state concentrations of about 2.1 and 4.1 $\mu\text{g I/cm}^2$ of geometric surface were reached on plates 1 and 2, respectively, when the iodine burden was between 3 and 4×10^{-4} Pa. These surface concentrations were of the same magnitude as those obtained in earlier experiments but were less than the maximum values inferred from those experiments. The specimen plates used in this experiment were made larger and were cut and prepared differently from those used in earlier experiments. We expected the ratio of adsorption surface to geometric surface to be less than the value of 30 to 60 inferred for the earlier specimens. This difference may, in part, account for the lower-than-maximum steady-state surface concentrations.

In phase 2 the temperature was reduced to 300°C. The steady-state concentrations of iodine on plates 1 and 2 increased to 2.5 and 5.2 $\mu\text{g I/cm}^2$, respectively, at an iodine burden of about 2.5×10^{-4} Pa. We concluded that the steady-state concentration of iodine at constant burden would have been about 50% greater at 300 than at 400°C.

In phase 3 the temperature of iodine saturator was reduced with the intent of lowering the iodine burden in the helium to 3 to 4×10^{-6} Pa. The actual burden could not be measured accurately, but we concluded that it did not exceed 1.4×10^{-5} Pa and could have been 3 to 4×10^{-6} Pa. With the specimen at 400°C, iodine desorbed to a steady concentration of 1.4 $\mu\text{g/cm}^2$ on plate 1. The flow of gas through the iodine saturator was stopped in phase 4 to reduce to practically 0 the iodine burden in the helium entering the furnace tube. However, this change produced no further reduction in the concentration of iodine on plate 1. The iodine on plate 2 had desorbed to a concentration of 3.5 $\mu\text{g/cm}^2$ and was continuing to decrease at the end of phase 4.

In phase 5 the temperature of the specimens was reduced to 200°C, but the iodine burden was kept at 3 to 4×10^{-6} Pa. The results were inconclusive. Iodine desorbed and then adsorbed on plate 1. The concentration on the surface was still increasing but had not quite reached the concentration at the end of phases 3 and 4 at 300°C when the adsorption test at 200°C was ended.

For phases 6 through 9, the iodine burden was reduced to 0, and the desorption of iodine was measured at 200, 300, 400, and 500°C. Steady, or nearly steady, concentrations of 1.0 and about 0.06 $\mu\text{g I/cm}^2$ were measured at 200 and 500°C, respectively, for plate 1. Steady values were not reached during the times spent at 300 and 400°C. Iodine desorbed from plate 2 about as it did from plate 1, but the measurements were too few to establish whether steady concentrations were reached.

In phases 10 and 11, we intended to return to the conditions of phase 1 and investigate the effect of moisture in the helium on the adsorption of iodine. The specimens were held at 400°C, and the iodine burden in the helium was maintained at about 3×10^{-4} Pa. This time, however, the concentration became steady at 0.4 $\mu\text{g/cm}^2$ on plate 1, only about 0.2 of the steady concentration at the end of phase 1. The concentration on plate 2 at this time was 1.2 $\mu\text{g/cm}^2$, 0.3 of the steady concentration at the end of phase 1, but it might still have been increasing. In earlier experiments, it was found that the adsorptive capacity of the steel sometimes decreased when a specimen was heated to higher temperature, usually to 600 to 800°C, to desorb iodine.

After the concentration of iodine on plate 1 had become steady, moisture was added to the helium. The moisture addition was uncontrolled, and difficulties with the equipment made it intermittent. The results are uncertain. In the two instances during which it appeared certain that moisture was being added, some of the iodine desorbed. It is interesting that after the first addition of moisture and slight desorption, iodine began to adsorb rapidly on both plates. The concentrations reached 4.1 and 5.1 $\mu\text{g I/cm}^2$ on plates 1 and 2, respectively, and were still rising when iodine desorbed following a resumption of the addition of moisture. Presumably, exposure to the moisture produced a change in the surface of the metal.

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2. FUELED GRAPHITE DEVELOPMENT

F. J. Homan and M. J. Kania

2.1 INTRODUCTION — F. J. Homan

The Fueled Graphite Development Program at ORNL continues as part of the national High-Temperature Gas-Cooled Reactor (HTGR) fuel development effort being conducted in cooperation with the General Atomic Company (GA). The ORNL portion of the program is focused on irradiation testing and specialized postirradiation examination (PIE) techniques and equipment. General Atomic's portion of the program includes interface with core designers and with the licensing activity and responsibility for the fuel performance model development. General Atomic also has responsibility for fuel manufacturing, including design, process development, and quality control.

During this reporting period a major program milestone was reached: a reference fuel system for the commercial HTGR was chosen. This system includes a Triso-coated fissile particle and a Triso-coated fertile particle. The fissile kernel is "dense UCO" (actually a mixture of UO_2 and UC_2), and the fertile kernel is ThO_2 . The tasks of qualifying and licensing the reference fuel system lie ahead. Our role is to continue providing services for the design, assembly, and operation of irradiation capsules and for the examination of irradiated fuel and to interpret results of the irradiation experiments. All these activities will be closely coordinated with GA's staff.

In addition to the cooperation with GA under the national HTGR fuel development program, a cooperation is in effect with the German Nuclear Research Center at Jülich [Kernforschungsanlage (KFA) Jülich]. The cooperation with KFA is part of the formal agreement between the U.S. and German governments for gas-cooled reactor development. Areas of work under the ORNL-KFA cooperation include fuel postirradiation examination and SiC coating characterization. Also included in the cooperation is a

personnel exchange. Currently a German scientist is assigned to the ORNL Fueled Graphite Development Program, and a summary of work performed during this reporting period is included in this chapter. During this reporting period, the ORNL assignee to KFA was working in the metals program rather than in the fuel development program.

The Fueled Graphite Development Program continues its organization into four subtasks: (1) Irradiation Testing, (2) Development Work, (3) Project Work Statement (PWS) Activities, and (4) Equipment Development and Maintenance.

2.2 IRRADIATION EXPERIMENTS — M. J. Kania

During this reporting period irradiation capsules HRB-15a and HRB-16 completed their scheduled irradiations. Both capsules contained only GA-produced fuel; their operating conditions are reported in Chap. 3. Postirradiation examinations continued on capsules HT-35, HRB-14, and HRB-15b and were initiated on HRB-15a. Irradiated microsphere gamma analyzer (IMGA), postirradiation gas analyzer (PGA), and microprobe examinations for the HT-35 fuel were delayed until mid-spring 1982. Results of the visual inspection of ORNL fuel from the loose particle crucibles and the irradiation-induced dimensional change data for the graphite components of capsule HT-35 are presented in Sect. 2.2.1. A final report on HRB-14 was written, and the abstract is presented in Sect. 2.2.2. For HRB-15b, gamma spectrometry results obtained with IMGA and fission gas retention results obtained with PGA are shown in Sect. 2.2.3 for the ORNL fuel.

2.2.1 Capsule HT-35 — M. J. Kania and T. N. Tiegs

Irradiation capsule HT-35 was a cooperative effort between ORNL and GA. Specimens prepared at ORNL occupied about 75% of the available test space for fertile particles, and GA specimens occupied the remaining space. In addition to the fertile fuel, ORNL tested Triso-coated low-enriched uranium (LEU) fissile particles in the driver rod positions along the length of the capsule. The objectives and a detailed description of capsule HT-35 are reported in refs. 1 and 2. The capsule was inserted into a target position in the High Flux Isotope Reactor (HFIR) on August 17, 1980, and was removed on November 19, 1980, after accumulating

2140.5 h of full reactor power (100 MW). Peak burnup achieved was 8.3% FIMA [fissions per initial (heavy) metal atom] for the fertile particles, and the peak accumulated fast neutron fluence was about 8.8×10^{25} neutrons/m² ($E > 29$ fJ). A detailed description of fuel burnup and neutron fluence accumulation is reported in ref. 2.

The capsule was disassembled in December 1980, and no unusual problems were encountered. The GA fuel specimens were stored in appropriately identified containers for future examinations. The eight GA piggy-back samples were shipped to GA in March 1981. The ORNL fertile particles were unloaded from their respective graphite crucibles and visually examined. Table 2.1 is a summary of the results from the visual examination. In general, the visual inspection revealed that most particles were intact after irradiation. Exceptions to this were particles from batches A-762 HT, A-935 HT, A-765 HT, and A-785 HT in the high-temperature crucibles and batch A-762 HT in the low-temperature crucible.

The driver fuel rods containing Triso-coated LEU particles were deconsolidated to obtain loose particles. The electrolyte solution used during the electrolytic deconsolidation was sampled and chemically analyzed for total thorium and uranium content. This procedure was carried out for each of the 20 driver rods deconsolidated. Only one of the analyses (from driver rod 14) showed appreciable levels of thorium and uranium above the detection limit. A full analysis and interpretation of the number of failed particles these data represent is under way.

Dimensional change measurements were made on the empty graphite magazines, empty graphite crucibles, and driver fuel rods. Figure 2.1(a) compares the irradiation-induced dimensional change data from the graphite crucibles of HT-35 with similar data obtained from previous HT irradiation capsules. The curve is a least-squares fit to the data shown in Fig. 2.1(b) [curve shown in Fig. 2.1(a) is the same as in Fig. 2.1(b)]. In the high-temperature (high-fluence) region, the agreement is good, but in the low-temperature (low-fluence) region, the data are slightly below those of previous experiments. The scatter in the HT-35 data is similar to that from other experiments. Figure 2.2(a) presents the dimensional change data for the HT-35 graphite magazines and compares it with similar data from previous HT irradiation capsules. The curve in Fig. 2.2(a) is a

Table 2.1. Results of visual examination on ORNL fertile fuel particles irradiated in loose-particle crucibles in capsule HT-35

Crucible	Particle batch	Intact particles		Comments
		Loaded	Counted in PIE ^a	
4	A-765 HT	82	81	One particle missing in visual examination
5	A-780 HT	90	90	
10	A-785 HT	104	104	
11	A-762 HT	126	126	Two particles broken with kernels and coating fragments visible
13	A-898 HT	101	101	
17	A-898 HT	79	79	
18	A-899 HT	80	80	
20	A-914 HT	75	75	
21	A-915 HT	77	77	
23	A-919 HT	76	76	
24	A-935 HT	78	76	Two bare kernels visible
26	A-961 HT	84	84	
27	A-901 HT	80	80	
29	A-765 HT	83	76	Two particles broken, with kernels and coating fragments visible
30	A-780 HT	81	81	
32	A-785 HT	81	76	Five particles broken, with no kernels visible
33	A-762 HT	79	75	Four broken, with no kernels or coating fragments visible
35	J-489	75	75	
40	A-899 HT	98	98	
42	A-914 HT	97	98	Extra particle added; assumed spilled during disassembly
43	A-915 HT	100	96	Four particles missing in visual examination
45	A-919 HT	99	98	One particle missing in visual examination
48	A-935 HT	84	82	Two particles missing in visual examination
49	A-961 HT	81	78	Three particles missing in visual examination

^aPostirradiation examinations.

Fig. 2.1. Irradiation-induced dimensional changes for graphite sample holders from HT-series capsules. (a) HT-35. (b) Previous HT capsules.

Fig. 2.2. Irradiation-induced dimensional changes for graphite magazines from HT-series capsules. (a) HT-35. (b) Previous HT capsules.

least-squares fit of the data shown in Fig. 2.2(b) [curve shown in Fig. 2.2(a) is the same as shown in Fig. 2.2(b)]. Again, only the high-temperature (high-fluence) dimensional change data are in agreement. The reason for disagreement in the low-temperature (low-fluence) dimensional change data for capsule HT-35 is not known. Figure 2.3 presents the dimensional change data for the driver fuel rods. The loose particles irradiated in the graphite crucibles and the individual particles obtained from driver rod deconsolidation will be investigated by IMGA, PGA, and microprobe examinations. This work is scheduled to begin in May 1982.

Fig. 2.3. Irradiation-induced dimensional changes for fuel rods irradiated in capsule HT-35.

2.2.2 Capsule HRB-14 — M. J. Kania and T. N. Tiegs

Descriptions of the capsule design and fabrication,³ in-reactor operation,⁴ and results of the postirradiation examinations⁴⁻⁶ have been reported. During the past reporting period a final report was written for HRB-14; the abstract of that report follows.⁷

Irradiation capsule HRB-14 was a shared experiment with the General Atomic Company (GA). One-half the space in the capsule was used to test GA fuel; the other half was used to reach two experimental objectives of the ORNL program:

- The primary objective was to conduct a systematic study of the irradiation-induced permeability of reference Biso-coated fertile particles. The hope was to measure as a function of the coating conditions the neutron exposure at which coatings became permeable to fission gas.
- A secondary objective was to establish a fuel product specification for Biso coatings. The specification would include the range of BAF_0 and density values for pyrocarbon coatings that would remain intact and gastight throughout irradiation to design exposure values in an operating HTGR.

To achieve these experimental objectives, 18 batches of Biso-coated fertile particles were fabricated, characterized carefully and completely, and irradiated to neutron exposures ranging from about one-half to the full exposure expected in a commercial high-temperature gas-cooled reactor (HTGR). Neither experimental objective was achieved, but much valuable information on gas permeability was obtained. The results of this experiment were instrumental in the decision to abandon the Biso particle concept in the U.S. program and to go instead to a Triso-Triso system. A summary of the results is given below.

- Large within-batch variations were observed in the properties of as-produced coatings. This variation is presumably due to the random movement of the particles in the coating furnace during coating so that "average" coating conditions convey very little meaning about the properties of individual particles.
- Large within-batch variations were observed in the fission gas retention in particles during irradiation. Again, this variation is presumably due to the large variation in the properties of the as-fabricated coatings.
- It is possible to fabricate Biso-coated particles that remain gastight throughout irradiation. But it is not possible to do so economically with large batch sizes and still have the failure fraction (fraction that loses fission gas during irradiation) below 10^{-4} .
- In addition to the large variation in gas retention properties among particles from the same batch, a large variation also occurred in the retention of metallic fission products (^{137}Cs , ^{110m}Ag).

- No preirradiation characterization procedure was successful in predicting the irradiation behavior of pyrocarbon coatings (gastightness). This failure is due to the large variation in properties mentioned earlier. Average values for such preirradiation characterization techniques as inert gas intrusion (Ne/He ratio), long-term chlorine leaching coupled with x-radiography, and measurements of density and porosity do not correlate well with irradiation performance. Such average values do not account for the significant portion of the population in the tails of the distributions.
- The most important coating property appears to be the degree of crystallite disorder as determined by BAF_0 measurements. This result is not a conclusion of this experiment but results from earlier work. The HRB-14 experiment merely confirmed this conclusion. The model used to design the HRB-14 experiment (showing a "window" of acceptable BAF_0 values between too high and too low where coatings are gastight in the as-fabricated condition and remain gastight throughout the irradiation) appears to be valid. Coatings with BAF_0 values too high (high crystallite disorder) are permeable in the as-fabricated condition and remain permeable throughout irradiation. Coatings with BAF_0 values too low (low disorder, high level of anisotropy) are gastight in the as-fabricated condition but during irradiation develop microcracks from stresses that develop from differential swelling.
- Triso-coated fertile particles irradiated in capsule HRB-14 retained both gaseous and metallic fission products much more successfully than did the Biso-coated particles.

2.2.3 Capsule HRB-15b — M. J. Kania and T. N. Tiegs

The objectives, fabrication, operation, and disassembly of capsule HRB-15b have been reported.⁸⁻¹⁰ Postirradiation examinations with IMGA and PGA systems were begun in this reporting period.

For each ORNL irradiation position, individual particles (population sizes of 46 to 100 particles) were examined with the IMGA system. The results are summarized in Table 2.2, which also compares measured and calculated $^{137}\text{Cs}/^{106}\text{Ru}$ activity ratios from each position. The agreement between measured and calculated ratios indicates retention of ^{137}Cs as illustrated in column 9 of Table 2.2. The ratio of the mean measured $^{137}\text{Cs}/^{106}\text{Ru}$ to the calculated value differs by no more than about 5% for

Table 2.2. Results of irradiated microsphere gamma analyzer (IMGA) examinations on ORNL fertile fuel irradiated in capsule HRB-15b

Capsule position ^a	Batch	Coating design	Observed ¹³⁷ Cs/ ¹⁰⁶ Ru activity ratio				Calculated ratio	Mean fraction observed/calculated (%)	Burnup (% FIMA) ^b	Fast neutron fluence $E > 29$ fJ (neutrons/m ²)
			Minimum	Maximum	Mean	Standard deviation (%)				
17 (79)	A-834 ^c	Triso	0.9078	1.191	1.002	6.59	0.9665	103.7	3.1	4.3×10^{25}
19 (46)	A-835 ^c	Triso	0.8648	1.207	0.9918	6.80	0.9660	102.7	3.2	4.4×10^{25}
25 (54)	A-836 ^c	Triso	0.8955	1.092	0.9774	5.46	0.9637	101.4	3.6	4.7×10^{25}
27 (100)	A-837 ^c	Triso	0.8577	1.090	0.9700	5.52	0.9630	100.7	3.8	4.8×10^{25}
61	A-824	Triso					0.9430		5.5	6.2×10^{25}
63 (70)	A-825	Triso	0.1266	1.179	1.066	11.48	0.9440		5.5	6.2×10^{25}
65 (75)	A-826	Triso	0.8758	1.031	0.9369	3.70	0.9445	99.2	5.6	6.3×10^{25}
69 (90)	A-827	Triso	0.8614	1.006	0.9354	3.33	0.9429	99.2	5.7	6.4×10^{25}
73	A-828	Triso					0.9418		5.8	6.4×10^{25}
75 (100)	A-833	Triso	0.8670	1.061	0.9642	3.75	0.9409	102.5	5.8	6.5×10^{25}
79 (100)	A-834 ^c	Triso	0.9044	1.099	0.9796	4.06	0.9400	104.2	5.9	6.5×10^{25}
83 (100)	A-835 ^c	Triso	0.8985	1.127	0.9779	4.00	0.9394	104.1	6.0	6.5×10^{25}
85 (100)	A-836 ^c	Triso	0.8971	1.086	0.9811	3.96	0.9383	104.6	6.0	6.5×10^{25}
89 (100)	A-837 ^c	Triso	0.9054	1.131	0.9881	3.87	0.9381	105.3	6.0	6.6×10^{25}
91 (80)	OR-2262HT	Biso	0.9037	1.019	0.9673	2.46	0.9378	103.1	6.0	6.6×10^{25}
95 (100)	A-765 ^d	Biso	0.9045	1.038	0.9623	2.69	0.9371	102.7	6.0	6.5×10^{25}
99 (100)	A-786 ^d	Biso	0.9067	1.053	0.9713	3.43	0.9375	103.6	6.0	6.5×10^{25}
109 (100)	J-482 ^e	Biso	0.9194	1.034	0.9702	2.67	0.9386	103.4	6.0	6.3×10^{25}
111 (84)	J-489 ^e	Biso	0.8783	1.035	0.9462	3.32	0.9388	100.8	5.9	6.3×10^{25}
115 (67)	J-491 ^e	Biso	0.9004	0.9973	0.9441	2.55	0.9400	100.4	5.9	6.2×10^{25}
119 (100)	J-649 ^e	Biso	0.8814	1.034	0.9489	2.79	0.9410	100.8	5.8	6.1×10^{25}
123 (100)	J-651 ^d	Biso	0.8958	1.071	0.9828	3.71	0.9426	104.3	5.7	6.0×10^{25}
155 (100)	A-765 ^d	Biso	0.9036	1.074	0.9870	3.70	0.9616	102.6	4.4	4.5×10^{25}
159 (100)	A-786 ^d	Biso	0.9102	1.120	0.9857	4.99	0.9636	102.3	4.2	4.3×10^{25}
179 (90)	J-651 ^d	Biso	0.9813	1.225	1.083	5.29	0.9741	111.2	2.8	3.2×10^{25}

^aNumbers shown in parentheses represent number of particles examined with IMGA.

^bFIMA, fissions per initial (heavy) metal atom.

^cTriso ThO₂ in HT-34.

^dBiso ThO₂ in HT-34 and HRB-14.

^eBiso ThO₂ in OF-2.

all positions except 179 at the end of the capsule. The general uncertainty of the numbers shown in column 9 of Table 2.2 ranges from 7% in the center to about 13% at the ends of the capsule. This uncertainty includes the error in the gamma counting as well as the uncertainties in the neutronics data used for the calculations. A detailed analysis of the examined population from each position is under way. In some cases the lower limit of the measured $^{137}\text{Cs}/^{106}\text{Ru}$ activity ratio (Table 2.2, column 4) is significantly lower than the mean ratio. This difference is evidenced by batch A-825 irradiated in position 63. The minimum value of 0.1266 compared with the mean value of 1.066 indicates that one or more particles from this batch have low ^{137}Cs inventories compared with their ^{106}Ru inventory.

Fission gas release measurements were made with the PGA system. Five to six particles were selected from each irradiation position of interest and their fission gas content subsequently determined. (Particles from irradiation positions 63, 65, 69, and 73 were not measured.) The actual inventory measurements for the stable krypton isotopes are presented in Sect. 2.5.2. A comparison between the PGA-measured data and the gas inventory available for release is presented in Fig. 2.4 (see ref. 6 for more details on the relevance of this type comparison). The inventory available for release was calculated from burnup and operating temperature data⁹ and takes into account uncertainties in kernel diameters and temperatures. The agreement is relatively good for all positions except 19 and 155. Position 19 contained particle batch A-835, a Triso-coated ThO_2 particle, which was also irradiated in capsule HT-34 (ref. 11). The performance of this particle as measured by ^{137}Cs retention was excellent in both the high- and low-temperature magazines of capsule HT-34. Position 155 contained particle batch A-765, a Biso-coated ThO_2 particle also irradiated in capsules HT-34 (ref. 11) and HRB-14 (ref. 6). In the long-term HRB-14 capsule (5124-h irradiation), this particle batch lost significant amounts of its cesium and fission gas. In the short-term HT-34 capsule (2685-h irradiation), this particle batch had deficient cesium inventory in the high-temperature (about 1350°C) magazine but not in the low-temperature (about 1150°C) magazine. Some particles from both

the high- and low-temperature magazines were observed to be deficient in fission gas inventory. Particles from irradiation positions 17, 25, 27, 85, 159, and 179 in HRB-15b are being evaluated, and the results will be incorporated into Fig. 2.4 when they are available.

A final report describing the irradiation performance of the Triso- and Biso-coated ThO_2 particles irradiated in capsule HRB-15b is now in preparation.

Fig. 2.4. Comparison between measured and predicted fission gas release from Biso- and Triso-coated fertile particles irradiated in capsule HRB-15b.

2.2.4 Capsule HRB-15a — M. J. Kania

Irradiation capsule HRB-15a contained fuel specimens supplied by GA (no ORNL fuel). The capsule was designed and assembled by ORNL and irradiated in HFIR. The scheduled eight-cycle irradiation of the capsule began in July 1980 and ended in January 1981. The operating history of HRB-15a is presented in Chap. 3. The entire capsule was shipped to GA in March 1981 for PIE. To complete the planned fuel investigations, GA has requested ORNL to perform certain examinations on selected specimens. These examinations include IMGA, x-radiography, metallography, and

electron microprobe examinations. The specimens of interest were sent to ORNL in August 1981, and the examinations began in mid-December 1981. This work is scheduled for completion in May 1982, at which time the ORNL results will be forwarded to GA.

2.3 DEVELOPMENT WORK

2.3.1 Pyrocarbon Coating by CO₂ Dilution — D. P. Stinton

A final paper summarizing the pyrocarbon coating effort by CO₂ dilution was accepted for publication by the American Ceramic Society.¹² The abstract of this paper follows.

Fuel particles for the High-Temperature Gas-Cooled Reactor (HTGR) are coated with layers of pyrolytic carbon to accommodate kernel swelling and to contain fission products, including gases, during irradiation. During the past ten years, the permeability and anisotropy of the outer pyrocarbon layer have been optimized for good irradiation performance. However, even coatings whose properties were optimized for permeability and anisotropy sometimes fail during irradiation testing. Nevertheless, one batch of particles produced in 1973 by depositing the highly dense pyrocarbon layer from a mixture of propylene and CO₂ in place of argon and helium has shown good performance in several irradiation tests. Because of these encouraging results, the current work was undertaken to determine the effect of CO₂ dilution on pyrocarbon coatings.

This paper concluded that the deposition of pyrocarbon coatings by use of CO₂ dilution produced more gastight coatings (as-produced) than deposition of coatings with helium, argon, or H₂ dilution. Eight CO₂-diluted coatings were irradiation tested to determine the influence, if any, of CO₂ dilution. Irradiation of this capsule (HT-35) has been completed, but no results on the CO₂-diluted coatings are yet available.

2.3.2 Silicon Carbide Characterization and Performance Assessment — R. J. Lauf and P. Krautwasser*

2.3.2.1 Examination of General Atomic SiC Batches — R. J. Lauf

Two SiC batches (6157-11-010 and 6252-12-COMP) were sent to ORNL to be characterized by techniques reported earlier.¹³ Table 2.3 gives the

*On assignment from Kernforschungsanlage Jülich, West Germany.

Table 2.3. General Atomic Company SiC coating data

Batch	6252-12-COMP ^a	6157-11-010
Kernel type	ThO ₂	UC _{0.3} O _{1.5}
Fabrication data		
Coater size, mm	240	240
Temperature, ^b °C	1650	1650
Coating gas	CH ₃ SiCl ₃	CH ₃ SiCl ₃
Diluent gas	H ₂	H ₂
Charge size, kg	~10 ^c	5.4
Coating rate, μm/min	0.32 ^c	0.26
Mean properties		
Thickness, μm	38	36
Density, Mg/m ³	3.22	3.22

^aComposite of 12 batches.^bControl temperature.^cAverage of the 12 batches.

coating conditions and batch characteristics of the two coating runs examined. The results of this work can be summarized as follows:

1. The stacking fault density as measured by x-ray diffraction is slightly higher in batch 6252-12-COMP. This result is not surprising because the coating rate of 6252-12-COMP was about 25% higher than that of 6157-11-010.
2. Grain size and shape, observed metallographically, suggest that these coatings would correspond to a nominal deposition temperature of about 1550°C in the small ORNL coater.
3. Transmission electron microscopy (TEM) showed that both coatings have a grain size of 1 to 3 μm in the tangential direction and about 2 to 6 μm in the radial direction. Some grains are approximately equiaxed, whereas others are more elongated in the growth direction. The stacking fault density is not as high as in the German batches examined (Sect. 2.4.1.1).

4. The overall microstructures of batches 6157-11-010 and 6252-12-COMP look fairly good. Neither coating shows structures that are believed to be related to poor fission product retention (e.g., heavily faulted grains leading to grain boundary disorder or long columnar grains giving a more direct diffusion path to the outside).

General Atomic has recently found that some particles in 6157-11-010 have an anomalous coating structure and poor fission product retention. The structure is seen by scanning electron microscopy (SEM) after chemical etching. The origin of this effect is unknown, and TEM studies of more coatings from this batch would probably be useful.

2.3.2.2 Raman Spectroscopy and Small-Angle X-Ray Scattering (SAXS) Studies on SiC Coatings — P. Krautwasser and G. M. Begun

Raman spectra describe the chemical composition of a material as well as the degree of crystallinity. For the Raman studies of SiC coatings, an argon ion laser with a 514.4-nm excitation line was used. A shift of the initial laser wavelength to a series of larger wavelengths (Stocke lines, Raman spectrum) is caused by the partial absorption and emission of the laser beam energy by the stimulation of vibrations and rotations of the investigated molecules. The energies absorbed correspond to the shifts in wavelength. The measurements were performed on single particles mounted on a hypodermic needle. Data were obtained with a Raman or HG-25 spectrophotometer containing a double monochromator with curved holographic gratings and photon counting electronics with a coded S-20 type photomultiplier. The spectrometer was controlled and the data stored with a Nicolet 1170 signal averager. To eliminate the plasma lines, a spike filter was used. The resolution was varied between 3 and 8 cm^{-1} .

The Raman spectra of SiC coatings indicate whether a coating is stoichiometric or contains excess silicon or excess carbon. The spectra also provide information about the degree of crystallinity of the coating.

Raman studies of SiC batches SC-472 to SC-489

These SiC coatings were initially produced to study the influence of SiC deposition temperature and coating rate on the Ag- and Pd-SiC interactions. In Fig. 2.5, the Raman spectra of SiC coatings with intermediate

ORNL-DWG 81-18098A

Fig. 2.5. Excess carbon increases and excess silicon decreases with increasing deposition temperature.

coating rates (0.50–0.75 $\mu\text{m}/\text{min}$) are shown as a function of deposition temperature. The lines at about 790, 970, and 1520 cm^{-1} belong to SiC. Carbon lines exist at 1367 and 1595 cm^{-1} . Excess (amorphous) silicon is indicated by the broad bump at about 400 to 500 cm^{-1} . As shown in Fig. 2.5 by the decrease of the silicon bump and the increase of the carbon line intensity, excess silicon decreases and excess carbon increases with increasing deposition temperature. The coatings derived at 1650°C are nearly stoichiometric. Only a very small amount of excess carbon is present, as seen by the blowup of the spectrum at high delta wave numbers. Figures 2.6 and 2.7 show that, with increasing coating rates, the amount of excess silicon increases and the amount of excess carbon in the SiC coating decreases.

The SiC coatings of particle batches SC-472 through SC-489 deposited on UO_2 kernels were simultaneously derived with SiC coatings deposited on inert kernels. The inert kernels were added to achieve a better fluidization of the UO_2 particles in the coater. Because of the difference in size (UO_2 particles, $\sim 550 \mu\text{m}$; inert particles, $\sim 900 \mu\text{m}$) and in density, the coating rate of SiC on UO_2 kernels was higher than the coating rate of SiC on inert kernels. According to the Raman spectra, this difference leads (depending on deposition temperature) to a higher silicon content or a lower carbon content in the SiC coatings of the UO_2 particles compared with the SiC of the inert particles.

Raman studies of ThO_2 archive particles from batches included in capsule HT-34

Figure 2.8 shows the Raman spectra of unirradiated SiC coating batches A-834 through A-837 with the highest coating rate on top and the lowest on the bottom. All coatings contain a high amount of excess carbon; no free silicon was found. (The O_2 line is caused by the air surrounding the sample during the measurement.) By comparing the intensities of the carbon lines at about 1360 and 1580 cm^{-1} with the intensities of the SiC lines at 790, 970, or 1510 cm^{-1} , we concluded that the coatings with the highest (A-835) and the lowest (A-834) coating rates show the lowest amount of excess carbon, followed by batch A-837. The SiC coating batch (A-836) with an intermediate coating rate contains by far

ORNL-DWG 81-18996A

Fig. 2.6. Excess silicon increases with increasing coating rate.

Fig. 2.7. Excess carbon decreases with increasing coating rate.

Fig. 2.8. Raman spectra of archive particles included in HT-34.

the highest amount of excess carbon. Ceramographic sections of these four particle batches show distinct variations in structural morphology from the inner to the outer side of the SiC coatings. An example is shown in Fig. 2.9 (batch A-834), which indicates that either the coating rate or some other production parameter was varied during SiC coating deposition. Because it is not clear whether or not the relationship between the coating rate and the amount of excess carbon in a coating is linear, comparison of mean coating rates of heterogeneous coatings with other material properties may be misleading.

Y-145654

10 μ m

Fig. 2.9. Ceramographic section of SiC coating batch A-834 showing structural variations from the inside to the outside of the SiC coating.

Raman studies of archive particles from batches included in capsule HT-35

UC₂ particles. As in the SiC coatings of the UO₂ particles, the SiC coatings of the UCO particle batches OR-2884H through OR-2888H (SC-534 through SC-538) contain excess carbon (Fig. 2.10). As expected, the amount of free carbon is highest in the coating with the lowest coating rate (Fig. 2.10 bottom).

Inert particles. In agreement with earlier findings, the amount of excess carbon in the SiC coatings of the inert particle batches OR-2879H through OR-2882H (SC-528 through SC-531) decreases with increasing coating rates. After irradiation, the SiC peak intensities are lower; in batch OR-2881H, the lines show about the same intensity after neutron exposures of 3.9×10^{25} neutrons/m² at 900°C and 8.8×10^{25} neutrons/m² at 1250°C ($E > 20$ fJ) (Fig. 2.11). Obviously, the higher defect production at the higher fluence of fast neutrons was compensated for by the higher defect annealing rate at the higher irradiation temperature of about 1250°C than at 900°C. Although the unirradiated coatings contained excess carbon, excess silicon is present in the coatings irradiated at 900°C.

Raman studies of particle batches from GA

Two sets of archive particle batches from GA were investigated:

- Five batches included in the German irradiation test R2-K13.
- Ten batches included in tests HRB-14, HRB-15b, HT-34, and P13P.

Production parameters and characterization data of these particle batches are listed in Table 2.4. The Raman spectra of the GA coatings show a strong variation in the intensity of the SiC line at about 970 cm⁻¹ (Fig. 2.12). The line intensity ratios I_2/I_1 of the two major SiC lines at 790 cm⁻¹ (I_1) and 970 cm⁻¹ (I_2) (Table 2.4) show that all batches produced in the 35-mm coater have I_2/I_1 values smaller than 0.11, whereas the batches derived in the 24-cm coater have a more or less pronounced second SiC peak ($I_2/I_1 > 0.29$). Except for one batch with a low coating rate, the second SiC peak in the other batches is absent. This situation indicates a poor long-range order in the SiC grains of these coatings. In

Fig. 2.10. Raman spectra of archive particles included in HT-35.

Fig. 2.11. Neutron-induced structural defects annealed in part at high irradiation temperatures.

Table 2.4. General Atomic (GA) SiC coatings studied by Raman spectroscopy, small-angle x-ray scattering (SAXS), and scanning electron microscopy (SEM)

Batch	Kernel		Coater size (mm)	Irradiation tests	SiC deposition temperature (°C)	SiC coating (μm/min)	SiC density (Mg/m ³)	SIF ^a	SEM ^b	Composition ^c	I_2/I_1 ^d	Fission product release ^e
	Composition	Diameter (μm)										
6252-15-010	ThO ₂	450	240	HRB-15b, HT-34	1650	0.32	3.22	2.8	1	0	0.35	Cs, H
6252-12T-04AW	ThO ₂	450	240	R2-K13	1650	0.33	3.22	2.9	1	0	0.54	
6252-12T-COMP ^f	ThO ₂	450	240	R2-K13	1650	0.32	3.22	3.0	1	0	0.37-0.57	
6252-12T-04B2W	ThO ₂	450	240	R2-K13	1650	0.33	3.22	3.2	1	0	0.36	
6157-11-0100	UCO	350	240	R2-K13	1650	0.26	3.22		1	0	0.29	
6157-11-0200	UCO	350	240	R2-K13	1650	0.25	3.22	3.2	1	0	0.51	
6152-01-010	UO ₂ LEU ^g	299	35	HRB-14, 15b	1700	0.14	3.21		1	0	0.05	
4161-01-031	UO ₂ HEU ^h	203	127	P13-P	1525	0.18	3.20	3.4	3	Si 1, C 1	0.11	Cs, L
6152-03-010	UO ₂ LEU	312	35	HRB-15b	1700	0.29	3.20	5.0	2	Si 2	0	Ag, L
6151-21-010	UO ₂ LEU	348	35	HRB-15b	1700	0.33	3.16	6.8	2	Si 3	0	Ag, H
6157-09-010	UCO LEU	368	35	HRB-15b	1700	0.33	3.16	7.5	2	Si 4	0	Ag, L

^aSiC inhomogeneity factor (SIF_{1.2 mrad}) measured at KFA-Jülich and ORNL.

^bSiC surface morphology as seen by SEM; 1, flat surface (approximately stoichiometric); 2, "cauliflower"-like surface (SiC with excess silicon); 3, very fine grains; SEM studies performed at ORNL and KFA.

^cRelative content of excess silicon or carbon. Data derived from Raman spectroscopy; content ranging from 0 (none, stoichiometric) to 4 (high).

^dLine intensity ratios I_2/I_1 from Raman lines at 790 cm⁻¹ (I_1) and 970 cm⁻¹ (I_2).

^eRelease of silver or cesium measured in out-of-pile annealing experiments at GA; H, high release; L, low release.

^fComposite of 12 batches.

^gLEU, low-enriched uranium.

^hHEU, high-enriched uranium.

Fig. 2.12. SiC line intensity at 970 cm^{-1} varies significantly.

contrast to the results on these GA batches, all SiC coatings derived in small coaters at ORNL or KFA-Jülich show a distinct SiC peak at 970 cm^{-1} . As an example of the relationship of Raman line intensity ratios with changes in SiC surface morphology, SEM pictures of the outer SiC surface of a batch with a high I_2/I_1 ratio (0.79, KFA-SMAUTO 468) and a batch with no second peak (GA-6151-21-010) are shown in Figs. 2.13 and 2.14. The SiC inhomogeneity factors (SIFs) (see Sect. 2.3.2.4) for these two batches also indicate a well-ordered microstructure for batch SMAUTO 468 (SIF = 1.1, Table 2.5) and a high concentration of structural defects or a poor SiC stoichiometry for batch 6151-21-010 (SIF = 6.8, Table 2.4).

In addition to the relationship of SiC surface morphology with peak intensity ratios, SEM studies also showed a good agreement of surface morphology with coating composition as determined by Raman spectroscopy. Depending on the coater size, stoichiometric SiC has either a smooth,

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Fig. 2.13. Scanning electron micrograph of the outer surface of SiC batch SMAUTO 468.

M-12610

Fig. 2.14. Scanning electron micrograph of the outer surface of SiC batch 6151-21-010.

Table 2.5. German SiC coatings studied at ORNL by Raman spectroscopy

Batch	Manufacturer	Kernel		SIF ^a	SEM ^b	Composition ^c	I_2/I_1 ^d	Fluidization Gas
		Composition	Diameter (μm)					
SPA3UASiCsO 2289	KFA	UO ₂	397	1.00	1	0	0.24	H ₂
SMAUTO 468	KFA	(Th,U)O ₂		1.11	1	0	0.79	H ₂
SPA3UASiCsO 2280	KFA	UO ₂	397	1.39	1, 2	0	0.35	50% Ar
SPA3UASiCsO 2277	KFA	UO ₂	397	1.39	1	0	0.27	H ₂
SPA3UASiCsO 2279	KFA	UO ₂	397	1.56	1, 2	0	0.24	H ₂
SPA3UASiCsO 2278	KFA	UO ₂	397	1.61	3	C, 2	0.28	50% Ar
SPA3UASiCsO 2283	KFA	UO ₂	397	1.78	2	Si, 1	0.38	50% Ar
EUO 1551	HOBEG	UO ₂	205	2.24	1, 4	0	0.45	H ₂
EO 1607	HOBEG	(Th,U)O ₂	494	2.29	4	0	0.43	H ₂
EO 819/820	HOBEG	(Th,U)O ₂	499	2.36	2, 4	Si, 1	0.22	H ₂
EO 249-251	HOBEG	UO ₂	616	3.22	2	Si, 1	0.28	H ₂
EO 403-405	HOBEG	UO ₂	195	4.17	4	0	0.38	H ₂
SPA3UASiCsO 2282	KFA	UO ₂	397	7.50	2	Si, 2	0.38	H ₂
DO 475	HOBEG	(Th,U)O ₂	630			0	0.27	H ₂
HT 150-167	HOBEG	(Th,U)O ₂	500			0	0.42	H ₂
EO 1674	HOBEG	(Th,U)O ₂	496			0	0.47	H ₂

^aSiC inhomogeneity factor (SIF_{1.2 mrad}) measured at KFA-Jülich.

^bSiC surface morphology as seen by scanning electron microscopy; 1, interlocked grains (approximately stoichiometric); 2, "cauliflower"-like surface (SiC with excess silicon); 3, microstructure with grain boundary gaps; 4, flat surface; SEM studies were performed at KFA.

^cRelative content of excess silicon or carbon; Si = silicon, C = carbon; content ranging from 0 (stoichiometric) to 4 (high); data derived from Raman spectroscopy.

^dLine intensity ratios I_2/I_1 , from Raman lines at 790 cm⁻¹ (I_1) and 970 cm⁻¹ (I_2).

dense surface (12–24-cm coater) or large, interlocked grains (5–8-cm coater). These structures are denoted as type 1 in Table 2.4. The "cauliflower"-like structure of SiC coatings containing amorphous silicon is referred to as type 2. Structure 3 shows a very fine grain size (batch 4161-01-031 in Table 2.4). This material is somewhat inhomogeneous and contains some excess carbon as well as some amorphous silicon.

The daughter batches from GA differ from their parent batches by density separation or heat treatment. Although the density separation had no effect on the Raman spectra, the one batch heat treated for 1 h at the SiC deposition temperature (1700°C) showed a distinct decrease in excess silicon, which was present in the parent batch. Out-of-pile annealing of irradiated particles for fission product release measurements may therefore have some effect on the SiC microstructure.

In general, a deviation from stoichiometric SiC leads to a lower coating density. An increase in excess silicon (GA batches) or excess carbon (ORNL batches) both resulted in decreasing SiC density.

Small-angle x-ray scattering (SAXS) studies on SiC batches from ORNL, GA, KFA-Jülich, and HOBEG — P. Krautwasser

Small-angle x-ray scattering measurements of five ORNL and German batches already measured with a Kratky system at KFA-Jülich were repeated with the 10-m SAXS facility at ORNL. A correlation between the data determined at ORNL and KFA-Jülich was established, and several additional batches from ORNL and GA were measured at ORNL.

The SIF derived from the SAXS measurements describes the concentration of defects ranging in size from 12 to 25 nm. This defect size seems to be indicative of the degree of inhomogeneity and deviation of stoichiometric SiC. The SiC deposition studies at KFA-Jülich showed a strong variation of SIF with deposition temperature and, at some deposition temperatures, with coating rate.^{14,15} High SIF values were obtained both at low and high deposition temperatures, whereas intermediate temperatures lead to low SIF values. As expected, the high values are in part caused by excess silicon or carbon present in the SiC coatings derived at these extreme deposition temperatures. Excess silicon or carbon leads to an electron density fluctuation within a coating and subsequently to a strong small-angle scattering.

A comparison of the SIF values with the amount of excess silicon (GA batches) or excess carbon (ORNL batches) shows good agreement.

The rating of GA batches, according to the SIF values in Table 2.4, agrees well with the content of free silicon. The more excess silicon or carbon in the SiC lattice or as precipitates on grain boundaries, the higher the SIF. Batches A-834 and A-835 (Fig. 2.8) of the ThO₂ particles discussed previously show a lower carbon content than do batches A-836 and A-837. This result is consistent with the SAXS data in that the SIF is 4.3 and 4.5 for the first two batches and 6.1 and 7.3 for the latter batches. The densities are 3.19 Mg/m³ for A-834 and A-835 and 3.16 and 3.18 Mg/m³ for A-836 and A-837, respectively. Although the difference in density is relatively small, it can be well distinguished with an error in density measurement of 2 kg/m³. Raman, SAXS, and density measurements therefore yield comparable results, confirming that the coatings of batches A-834 and A-835 are very similar, although one batch had the lowest coating rate, the other the highest.

2.3.2.3 Transmission Electron Microscopic Studies of Fission Product Interactions — R. J. Lauf

To study fission product-silicon carbide interactions, particles of UO₂ or UC₂ are doped with fission product elements before coating and are then held in a thermal gradient for as long as several thousand hours.¹⁶ Examination of the SiC coatings by transmission and analytical electron microscopy after annealing shows that the silver behaves differently from the palladium group.

Palladium group

Figure 2.15 shows the typical SiC microstructure as deposited at 1500°C at 0.7 μm/min. The SiC coating from a UC₂-Mo-Ru-Rh-Pd particle is shown in Fig. 2.16 after annealing 260 h at 1597°C in a thermal gradient of 275°C/cm. Small (<1 μm) nodules of a noble metal compound occur along SiC grain boundaries. Restructuring of the SiC behind the nodules does not appear to be significant, suggesting that the nodules move by dissolving SiC at the leading edge and forming SiC at the trailing edge.

Fig. 2.15. Typical SiC microstructure for coating deposited at 1500°C at coating rate of 0.7 $\mu\text{m}/\text{min}$ (transmission electron micrograph).

Energy-dispersive x-ray analysis of the nodules indicated Pd, Rh, U, and Si. This finding is consistent with electron microprobe results, except that prior observations were unable to differentiate palladium and rhodium because of overlapping $L\alpha$ lines. This analysis with the JEM-100CX analytical electron microscope resolved the palladium and rhodium $K\alpha$ lines.

A few larger nodules observed in this specimen had more equiaxed shapes and appeared to contain only uranium and silicon. Migration of uranium is seen only in carbide-based fuel particles.

Fig. 2.16. Silicon carbide coating from Triso-coated UC_2 particle doped with Mo, Ru, Rh, and Pd and annealed for 260 h at $1597^\circ C$ in a thermal gradient of $275^\circ C/cm$ (transmission electron micrograph).

Silver

In concentrations typical of end-of-life fuel, silver does not attack the SiC or form second phases as does the palladium group. However, silver is sometimes released from particles even when the SiC layer is intact.¹⁷ Figure 2.17 shows the microstructure of the SiC coating from a UO_2 -Ag particle after annealing for 2000 h at $1450^\circ C$ in a thermal gradient of $275^\circ C/cm$. Note the similarity to the as-deposited structure. Neither second phases nor grain boundary films are observed in this coating. Energy-dispersive x-ray (EDX) analysis detected a large concentration of silver accumulating at the inner surface of the SiC, but no silver was detected along several grain boundaries probed with an electron spot size of about 300 nm. The kinetics of silver transport through SiC suggest a

Fig. 2.17. Microstructure of SiC coating from Triso-coated UO_2 particle doped with silver and annealed for 2000 h at 1450°C in a thermal gradient of $275^\circ\text{C}/\text{cm}$ (transmission electron micrograph).

grain boundary mechanism.¹⁷ The current results imply that, if a grain boundary layer of silver (or an Ag-Si alloy) exists in this sample, it is exceedingly thin. Future work is planned with lattice fringe imaging to try to distinguish this layer.

2.3.2.4 Silicon Carbide Fabrication, Microstructure, and Performance Correlations — R. J. Lauf, R. L. Pearson, and T. B. Lindemer

The fabrication-microstructure correlations were completed and reported in last year's annual report.¹⁸ The out-of-reactor microstructure-performance correlations were completed this year and a report was prepared.¹⁹ An abstract of the report appears below. The in-reactor performance correlations with microstructure must await examination of fuel irradiated in capsule HT-35.

The interactions of the simulated fission products palladium, the rare earths, and silver with the silicon carbide (SiC) in Triso-coated, high-temperature, gas-cooled reactor (HTGR) particles were investigated out of reactor in the temperature range 1500 to 1780 K. Both palladium and the rare earths interact in proportion to time (rather than the square root of time) up to 8600 h (0.98 year), the time limit of this study. Grain-boundary mechanisms are suggested. Effects of the SiC microstructure on the palladium reaction rate were minimal in comparison to the temperature effects. Silver interactions with SiC were observed but could not be quantified.

In a second set of experiments, the addition of silicon to carbide-containing kernels resulted in retention of U-Pd-Si-C compounds in the kernel. These prevented the reaction of palladium with SiC at temperatures of 1670 to 1920 K as long as rare earth carbides were not present.

2.3.3 Dense UCO Kernel Development — D. P. Stinton

The reference fissile particle for the HTGR contains a highly dense microsphere with a composition of UO_2 and UC_2 . A process was developed several years ago to convert urania plus carbon microspheres produced by internal gelation to the desired composition. During 1980 dense UCO microspheres were produced by the external gelation process from 7%-enriched uranium for use as the driver fuel in irradiation test HT-35. During sintering of these particles, significant cracking and formation of internal voids occurred. We worked with GA this year to improve the processes of forming and sintering microspheres produced by external gelation. Forming conditions were varied by GA personnel in a systematic way to determine the optimum combination of variables. Dried microspheres were shipped to ORNL and sintered with numerous variations of the sintering schedule. Because of the cooperative effort, a process using a 0.13-m-diam (5-in.) rotary graphite furnace was developed for producing kilogram quantities of dense UCO microspheres. Quality microspheres can currently be produced; however, further modifications to the processing conditions should yield a product of improved quality.

2.4 PROJECT WORK STATEMENT ACTIVITIES

This work is being performed under the Fuel, Graphite, and Fission Product Subprogram, which is one of several subprograms included under the

Umbrella Agreement between the United States and the Federal Republic of Germany (FRG). The fuel development work under this subprogram is being done by ORNL, GA, and KFA. Progress under the active project work statements (PWSs) during 1981 is described.

2.4.1 PWS FD-10 — P. Krautwasser and R. J. Lauf

2.4.1.1 Preliminary Characterization of German SiC Coatings — R. J. Lauf and P. Krautwasser

German SiC batches EO-403/405, EO-1607, and SPA3-2289 were examined according to techniques reported earlier.¹³ The major conclusions of this study are

1. All three SiC batches contain numerous stacking faults as observed by TEM. The stacking-fault density can also be observed qualitatively by x-ray diffraction. Batch SPA3-2289 has the least amount of faulting, and EO-403/405 has the heaviest faulting; EO-1607 is almost as heavily faulted as EO-403/405.

2. Grain boundaries are more disordered near highly faulted areas. Batch SPA3-2289 had grain boundaries somewhat smoother than those of EO-1607 or EO-403/405.

3. None of the coatings examined contained striations or numerous voids. Their overall densities were fairly good on the basis of TEM observations. A few voids observed at grain boundaries in two of the batches were roughly polygonal in outline.

4. The large, columnar grain structure suggests a deposition temperature comparable to a nominal 1650°C in the small ORNL coater. The stacking-fault densities suggest moderately high coating rates for EO-1607 and EO-403/405 and a slightly lower rate for SPA3-2289.

5. Raman spectra of batches SPA3-2289 and EO-1607 indicate a fairly stoichiometric SiC with a high degree of crystallinity. The variation of the peak intensities at 970 cm^{-1} suggests a difference in the long-range order of the SiC microstructures. Three batches were homogenized to form the composite batch EO-403/405. The Raman spectrum of one of these batches differs from those of the other two batches by showing excess carbon and silicon and having a low line intensity at 970 cm^{-1} . In contrast, the other batches in the composite appear to be stoichiometric.

6. Small-angle x-ray scattering measurements at ORNL and KFA-Jülich on selected coating batches gave a qualitatively consistent ranking of homogeneity. The concentration of defects in the 12- to 24-nm range is lowest in SPA3-2289 and highest in EO-403/405. Quantitative comparisons between ORNL and KFA SIF values have not yet been established.

7. With "composite" batches, SAXS probably gives a more statistically reliable sampling than does the TEM examination of a limited number of SiC fragments.

2.4.1.2 Further Studies of German SiC Batches — P. Krautwasser and G. M. Begun

In addition to the investigation of the three German batches shipped to ORNL in 1980, Raman spectroscopy was performed on 14 SiC coating batches included in the German SiC Characterization Program. Data on these batches are listed in Table 2.5.

Batch SMAUTO 468 has the highest I_2/I_1^* value (0.79) observed thus far. This observation means that the long-range order and the crystallinity in the SiC coating must be high, in agreement with SEM results showing a dense structure of large interlocked grains. Otherwise, there is no consistent relationship between the SIF and the Raman spectra. This situation may be due to simultaneous variations in coater size, deposition temperature, and coating rate as well as in kernel size.

2.4.2 PWS FD-12 — M. J. Kania, F. J. Homan, and B. A. Thiele[†]

The purpose of PWS FD-12 was the quantification of irradiation-induced permeability in pyrocarbon coatings. During this reporting period, all experimental investigations were completed, and a final report was written.⁶ The abstract and conclusions of that report are given below.

Two U.S. irradiation experiments were planned to provide information to supplement data from the German program on irradiation-induced permeability in pyrocarbon coatings. We hoped that the data from both programs could be combined to define the onset of neutron-induced permeability in a variety of

*See Sect. 2.3.2.2 for a discussion on I_2/I_1 values.

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Biso coatings produced with different process variables (coating temperature, coating gases, and coating rates). Our effort was not successful. None of the preirradiation characterization procedures adequately predicted irradiation performance. A large amount of within-batch scatter and significant within-batch variation in coating properties were observed in the data. Additional preirradiation characterization might result in a procedure that could successfully predict irradiation performance, but little can be done about the within-batch variation in coating properties. This variation is the result of random movement of particles within the coating furnace during pyrocarbon deposition.

1. Large within-batch scatter occurs for both as-produced coating properties and fission gas retention capabilities for Biso-coated fertile particles.

- It is possible to fabricate pyrocarbon coatings that are gastight in the as-produced condition and that remain gastight throughout the irradiation to target fluence and burnup levels. However, it is not possible to produce in sufficiently large batches coatings such that all but a very small fraction (10^{-4} or less) of the particles will remain gastight to full exposure.
- The large range of coating properties in the as-produced condition is responsible for the large scatter in fission product (both fission gas and cesium) retention fractions. Reliance on average values of preirradiation characterization without proper consideration of the statistical variation in these properties can lead to overly optimistic performance predictions.

2. No preirradiation characterization technique is available to predict how pyrocarbon coatings will perform during irradiation.

- Existing techniques might be useful if the scatter of values were not so large. But decisions (permeable or nonpermeable) are made on the basis of average values; when the average values lie close to the critical point (such as Ne/He values of 0.3), significant portions of the particle population lie on both sides of the critical point.
- The most important property for predicting irradiation performance is crystallite disorder as determined by BAF_0 measurements. The currently specified value (less than 1.05) is based on coatings surviving neutron-induced cracking of the pyrocarbon. Coatings with BAF_0 values at or below this specified value do not crack under irradiation, but they may become gas permeable. To make coatings with BAF_0 values below 1.05 yet retain adequate pyrocarbon density is very difficult.

3. Cesium retention in Biso particles during irradiation corresponds closely to fission gas retention. A small number of particles retain cesium, and a large within-batch variation occurs in the fraction of cesium retained. It is not uncommon to see particles that retain cesium but lose fission gas.

2.4.3 PWS FD-13 — M. J. Kania, F. J. Homan, and A. W. Mehner*

The objective of PWS FD-13 was to compare PIE techniques used in the United States and in the Federal Republic of Germany to measure statistically significant failure fractions of irradiated coated-particle fuels. The PWS specifically called for comparing results obtained with the IMGA system²⁰ at ORNL and the postirradiation annealing and autoradiography (PIAA) system²¹ at KFA-Jülich. Two particle batches were selected for comparison: (1) batch EO-819/820, a (Th,U)O₂ kernel with a Triso coating irradiated in experiment BR2-P21 (ref. 22) of the FRG fuel development program, and (2) batch A-601, a weak-acid resin (WAR)-derived UC_xO_{1-x} kernel with a Triso coating irradiated in experiment OF-2 (ref. 23) of the U.S. program.

A final report²⁴ of the activity under this PWS is being reviewed at KFA-Jülich. The major conclusion reached through completion of PWS FD-13 is that the postirradiation examination procedure employing the IMGA system is superior to the PIAA method for measuring statistically significant failure fractions. Although both systems have features in common (including capacity for examining large-particle populations, requirements for access to hot-cell facilities, and physically separating failed particles from nonfailed particles), the IMGA method was judged superior. We therefore recommend that the method using the IMGA system be adopted to perform irradiated coated-particle fuel performance evaluations. This conclusion and recommendation is based on

1. Failure fraction results measured on standard particle batches with IMGA were more accurate and showed qualitative agreement with gas release data.

2. The IMGA is a fully developed, automated system used in routine postirradiation fuel examinations, but PIAA is not.

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3. The IMGA allows for fission product identification and quantification of fission product release, but PIAA does not.

4. Particle failure fractions determined with IMGA are independent of particle coating type and failure mechanism but are not with PIAA.

5. Although IMGA has a high initial cost, the degree of sophistication and automation increases throughput enough that operating costs are lower than those for PIAA.

2.4.4 PWS FD-20 — M. J. Kania and G. A. Moore

The objective of PWS FD-20 on fuel development is to measure statistically significant failed-particle fractions on FRG fuels. Through this PWS, irradiated candidate High-Temperature Reactor (HTR) fuel specimens from the FRG fuel development program are shipped to ORNL, where they undergo PIE. The IMGA system at ORNL is used to measure failed-particle fractions.

Irradiated fuels from three experiments in the FRG program were selected as the first samples to be examined. The fuel selected is of one type, a (Th,U)O₂ kernel with a high-temperature isotropic (HTI) Biso coating, derived from methane. The three irradiation experiments had similar operating temperatures but accumulated different levels of fast neutron fluence. General information on these fuel samples is in Table 2.6.

Table 2.6. German fuel specimens for PWS FD-20

Irradiation experiment	Particle batch	Irradiation temperature (°C)	Fast neutron fluence ($E > 16$ fJ)
FRJ2-P22, compact 1	EO-1232-1234	950-1100	$< 1 \times 10^{25}$
R2-K10, sphere 3	EO-1084	900-1050	3.4×10^{25}
HFR-K1, sphere B2	EO-1232-1234	950-1100	5.0×10^{25}

The IMGA examinations have been completed on all three HTI-Biso-coated (Th,U)O₂ particle batches. A total of 1647 particles from batch EO 1232-1234 irradiated in experiment FRJ2-P22, compact 1-3 (ref. 25), was examined. From experiment HFR-K1, sphere B2 (ref. 26), 1977 particles from batch EO 1232-1234 were examined. From experiment R2-K10, sphere 3 (ref. 27), 669 particles from batch EO 1084 were examined. Counting times were 120 s per particle of analog-to-digital converter (ADC) live-time for FRJ2-P22, 180 s per particle for HFR-K1, and 1000 s per particle for R2-K10. The long counting time was necessary for fuel from experiment R2-K10 because of the period of time between IMGA examination and discharge of the fuel from the reactor (November 18, 1974). Even with the 1000 s per particle live-time, counting error for the fission product ¹⁰⁶Ru exceeded 12% of the mean value.

Results from analysis of the IMGA examinations to determine the activity ratios ¹³⁴Cs/¹⁰⁶Ru, ¹³⁷Cs/¹⁰⁶Ru, and ¹³⁴Cs/¹³⁷Cs are shown in Table 2.7 for each experiment. Also presented are the number of particles examined from each fuel element and the standard deviation (assuming "normallike" distribution) of the activity ratio for the total particle population.

Figure 2.18 shows the IMGA results for HFR-K1, sphere B2, in histogram form for the activity ratios ¹³⁴Cs/¹⁰⁶Ru, ¹³⁷Cs/¹⁰⁶Ru, and ¹³⁴Cs/¹³⁷Cs. Each of the three histograms has a large normal-looking distribution of particles in the right half. This distribution contains the majority of the particles examined. To the left of this distribution are significant numbers of particles with much lower activity ratios. From the ¹³⁴Cs/¹⁰⁶Ru ratio, 46 of the 1977 particles examined have deficient ¹³⁴Cs inventories compared with ¹⁰⁶Ru; from the ¹³⁷Cs/¹⁰⁶Ru ratios, 48 of the 1977 have deficient ¹³⁷Cs inventories compared with ¹⁰⁶Ru; and from the ¹³⁴Cs/¹³⁷Cs ratio, 10 of the 1977 have low ¹³⁴Cs inventories compared with ¹³⁷Cs. Concluding from the ¹³⁴Cs/¹⁰⁶Ru and ¹³⁷Cs/¹⁰⁶Ru activity ratios, 46 to 48 particles of the 1977 examined are failed.

The ten particles with low ¹³⁴Cs/¹³⁷Cs ratios are a subset of the set of particles with low Cs/Ru ratios. We felt before that the ¹³⁴Cs/¹³⁷Cs ratio might be indicative of fission gas release early in life because

Table 2.7. Selected activity ratios measured during IMGA examination of high-temperature isotropic Biso-coated (Th,U)O₂ particles under PWS FD-20

Experiment element	Particle batch	Activity ratio	IMGA data ^a /(fraction of mean value)			Standard deviation ^b (%)	Population size
			Minimum	Maximum	Mean		
FRJ2-P22/1-3	EO 1232-1234	¹³⁴ Cs/ ¹⁰⁶ Ru	1.278 (0.886)	1.640 (1.137)	1.443 (1.000)	4.24	1647
HFR-K1/B2	EO 1232-1234	¹³⁴ Cs/ ¹⁰⁶ Ru	1.399 (0.507)	3.143 (1.138)	2.761 (1.000)	6.11	1977
R2-K10/3	EO 1084	¹³⁴ Cs/ ¹⁰⁶ Ru	6.050 (0.667)	15.65 (1.752)	8.931 (1.000)	17.33	669
FRJ2-P22/1-3	EO 1232-1234	¹³⁷ Cs/ ¹⁰⁶ Ru	3.406 (0.916)	4.154 (1.118)	3.717 (1.000)	2.97	1647
HFR-K1/B2	EO 1232-1234	¹³⁷ Cs/ ¹⁰⁶ Ru	3.310 (0.633)	5.823 (1.115)	5.226 (1.000)	4.93	1977
R2-K10/3	EO 1084	¹³⁷ Cs/ ¹⁰⁶ Ru	64.52 (0.685)	152.7 (1.622)	94.14 (1.000)	16.27	669
FRJ2-P22/1-3	EO 1232-1234	¹³⁴ Cs/ ¹³⁷ Cs	0.360 (0.926)	0.423 (1.089)	0.388 (1.000)	3.07	1647
HFR-K1/B2	EO 1232-1234	¹³⁴ Cs/ ¹³⁷ Cs	0.360 (0.633)	0.569 (1.077)	0.528 (1.000)	2.83	1977
R2-K10/3	EO 1084	¹³⁴ Cs/ ¹³⁷ Cs	0.08321 (0.877)	0.1050 (1.107)	0.09486 (1.000)	5.36	669

^aIrradiated microsphere gamma analyzer (IMGA) data corrected back to discharge date from reactor for each experiment.

^bStandard deviation calculated by assuming "normallike" activity ratio distribution.

Fig. 2.18. Activity ratios derived during IMGA analysis of high-temperature isotropic Biso-coated (Th,U) O_2 particle batch EO 1232-1234 irradiated in sphere B2 of experiment HFR-K1.

^{133}Xe is a precursor of ^{134}Cs . However, we have not successfully correlated $^{134}\text{Cs}/^{137}\text{Cs}$ ratio with fission gas loss as measured with the PGA system.⁶ This lack of a correlation might mean only that the fission gas loss from the particles studied occurred late in life. However, we still feel that valuable information about particle performance is hidden in the $^{134}\text{Cs}/^{137}\text{Cs}$ ratio measurement; therefore, we continue to collect and report these data.

Figure 2.19 shows the IMGA results from compact 1-3 of FRJ2-P22 in histogram form for the activity ratios $^{134}\text{Cs}/^{106}\text{Ru}$, $^{137}\text{Cs}/^{106}\text{Ru}$, and $^{134}\text{Cs}/^{137}\text{Cs}$. In contrast to the data shown in Fig. 2.18, all the particle data here appear to belong to the same normal-looking distribution. Neither of the three histograms has a significant number of particles located to the left of the major distribution. It should be noted that the total histogram ranges presented in Fig. 2.19 cover the same range in activity ratio as do the major distributions shown in Fig. 2.18. For example, in Fig. 2.18 the major distribution of the $^{137}\text{Cs}/^{106}\text{Ru}$ activity ratio lies from channel 28 through channel 50 and corresponds to a range of 90.8 to 111.5% of the mean ratio for the batch. In Fig. 2.19 the total

Fig. 2.19. Activity ratios derived during IMGA analysis of high-temperature isotropic Biso-coated (Th,U) O_2 particle batch EO 1232-1234 irradiated in compact 1-3 of experiment FRJ2-P22.

range shown for the $^{137}\text{Cs}/^{106}\text{Ru}$ ratio, channels 1 through 50, is 91.6 to 111.8% of the mean ratio. Similar comparisons also exist for the $^{134}\text{Cs}/^{106}\text{Ru}$ and $^{134}\text{Cs}/^{137}\text{Cs}$ activity ratios. From this comparison, we conclude that no particles were observed as having low ^{134}Cs or ^{137}Cs inventories compared with ^{106}Ru for fuel irradiated in compact 1-3 of experiment FRJ2-P22. This result is probably due to the low accumulated neutron fluence of $<1 \times 10^{25}$ neutrons/ m^2 (see Table 2.6).

The $^{134}\text{Cs}/^{106}\text{Ru}$, $^{137}\text{Cs}/^{106}\text{Ru}$, and $^{134}\text{Cs}/^{137}\text{Cs}$ activity ratios derived from the IMGA examination of sphere 3 from R2-K10 are shown in Fig. 2.20. The $^{134}\text{Cs}/^{106}\text{Ru}$ and $^{137}\text{Cs}/^{106}\text{Ru}$ distributions are extremely broad, as evidenced by their large standard deviations of 17.3 and 16.3%, respectively. This situation is a direct result of the poor counting statistics for the nonvolatile fission product ^{106}Ru . The $^{134}\text{Cs}/^{137}\text{Cs}$ activity ratio indicates that the differences between ^{134}Cs and ^{137}Cs inventories for all particles examined were not so large. For particle batch EO 1084 contained in this fuel sphere, the IMGA results are not of sufficient value to make an accurate determination of fuel performance.

Fig. 2.20. Activity ratios derived during IMGA analysis of high-temperature isotropic Biso-coated (Th,U) O_2 particle batch EO 1084 irradiated in sphere 3 of experiment R2-K10.

The difference in performance of particle batch EO 1232-1234 exhibited in experiments FRJ2-P22 and HFR-K1 can be translated into a failure fraction for each experiment. Figure 2.21 (ref. 28) provides a relationship between the number of particles examined, the number of failed particles, and the failure fraction at a confidence level of 95%. The data for sphere B2 of HFR-K1 (48 failed of 1977 examined) yield a failure fraction of about 3×10^{-2} at a 95% confidence level. In a similar manner, the data from compact 1-3 of FRJ2-P22 (zero failed of 1647 examined) yield a failure fraction of about 1.8×10^{-3} at a confidence level of 95%. The difference in failure fraction in the two experiments is probably due to the higher accumulated neutron fluence in the HFR-K1 experiment.

A visual examination was performed on the failed particles of batch EO 1232-1234, which were physically separated from the nonfailed particles during the IMGA examination of sphere B2 of HFR-K1. Photographs taken during visual examination are presented in Fig. 2.22. Figure 2.22(a) shows the portion of those particles that were detected as having low $^{134}\text{Cs}/^{106}\text{Ru}$ ratios but normal $^{137}\text{Cs}/^{106}\text{Ru}$ ratios. Of the four particles

shown, two indicate cracks in the outer pyrocarbon layer, one still contains a portion of the overcoating material not removed during deconsolidation, and the fourth particle has a "normal" particle appearance. Figure 2.19(b) shows a portion of particles detected as having low $^{134}\text{Cs}/^{106}\text{Ru}$ and $^{137}\text{Cs}/^{106}\text{Ru}$ activity ratios. Noticeable in this photograph are a bare kernel and particles with cracked pyrocarbon layers and with portions of the overcoating material still attached to the outer pyrocarbon layer. The appearance of these particles is similar to that reported²⁶ by the Austrian research center at Seibersdorf. The dominant failure mechanism appears to be neutron-induced cracking of the outer pyrocarbon layer.

Fig. 2.21. Number of examinations required to predict failure fractions at the 95% confidence level when various numbers of failed particles were detected.

(a)

R-77121

(b)

1 mm

Fig. 2.22. Photographs taken during visual examination of particle batch EO 1232-1234 irradiated in sphere B2 of experiment HFR-K1. Nominal irradiation conditions: operating temperature, 900–1150°C; burnup, 11% fissions per initial (heavy) metal atom; and fast neutron fluence, 5×10^{25} neutrons/m² ($E > 16$ fJ). (a) Low $^{134}\text{Cs}/^{106}\text{Ru}$ and normal $^{137}\text{Cs}/^{106}\text{Ru}$. (b) Low $^{137}\text{Cs}/^{106}\text{Ru}$ and $^{137}\text{Cs}/^{106}\text{Ru}$.

2.5 EQUIPMENT DEVELOPMENT AND MAINTENANCE — M. J. Kania

Three major pieces of equipment for use with PIE of fuel from irradiation capsules have been developed over the past few years. These items of equipment are unique in the HTGR community in the United States, and in some cases the capabilities are unique in the world. Although this equipment is in routine use in the examination of HTGR fuel particles, we are continuing to develop and upgrade the capabilities to provide more and better quality information.

2.5.1 Irradiated Microsphere Gamma Analyzer — M. J. Kania and G. A. Moore

During the past reporting period, the IMGA system has operated reasonably well. Coated particles from irradiation capsules HT-34, HRB-14, and HRB-15b and from FRG experiments FRJ2-P22, HFR-K1, and R2K10 have been examined. In addition, selected particles from capsule HRB-15b were examined with IMGA in an effort to resolve discrepancies in HFIR cross-section data sets and fluxes used in our calculational codes.

2.5.1.1 IMGA Maintenance

A problem that existed for several years with the singularizer of the automated particle handler was isolated and eliminated during this reporting period: the five small-diameter holes in the singularizer drum that serve as particle pickup points would become plugged with pyrocarbon dust. Thus, no irradiated particles would be detected, and an inert limit error would terminate the IMGA examination routine. Such an error would occur after loading 200 to 300 particles from the hopper of the singularizer unit. To unplug the holes, it was necessary to stop the IMGA examination, disassemble the singularizer, and remove the drum from the IMGA cubicle. This process was extremely time consuming and reduced the IMGA examinations to an upper limit of about 300 particles.

The problem was identified as the lack of air flow to the interior of the singularizer drum. A small air jet pushes off each particle from the drum and at the same time sweeps out the pickup hole for any dust accumulated there. Without adequate air flow to the drum interior, dust accumulated in the holes until they eventually became plugged. The solution to the problem was simply to maintain the proper air flow to the singularizer during the entire IMGA operation. Occasionally the holes will still

become plugged, but a slight increase in the air pressure to the drum interior will free them. The elimination of this problem returns IMGA to operating unassisted for long periods of time.

In addition, maintenance problems with peripheral portions of the system were encountered, specifically the PDP 11/05 computer and the Ge(Li) gamma-ray detector. The problems with the computer were with properly addressing the central processor unit memory. A leak was detected in the cryogenic LN₂ dewar and electronic components in the detector preamplifier of the Ge(Li) detector. Downtime for the IMGA system was approximately 40% for the year because of these maintenance problems.

2.5.1.2 New Equipment Acquisition

Two recent acquisitions for the IMGA system have upgraded it significantly. They include a new input/output (I/O) terminal, which allows high-speed data transfer and provides high-quality printed hard-copy output, and a new backup data storage device, which contains both a fixed-disk drive unit and a removable floppy-disk drive unit in the same device. In the new data storage equipment, a DSD 880 device replaces the DataPacer installed with the original equipment. The terminal is fully operational now, but we are awaiting a new system software assembly to provide full use of the data storage device.

2.5.1.3 Resolution of HFIR Cross Sections

A comparison between measured and calculated fission product inventories on specific fuel particles from capsule HRB-15b was performed. The measured data were obtained from IMGA analysis on single particles irradiated in positions 50 and 148 of capsule HRB-15b. The calculated results were based on the best available data on integral flux and cross sections at ORNL and GA for the HFIR Removable Beryllium Test Facility. The ORNL neutronics data were obtained from flux monitor experiments in the HFIR,²⁹ and the GA data were obtained on similar-type experiments but analyzed by the GA staff.³⁰

The original fission product inventories derived with IMGA were corrected to account for a source-to-detector geometry error present in the initial data.^{6,31} A correction from the initial 55-cm recorded

distance to the correct 45-cm distance was necessary. This task was simple because IMGA software includes such a provision in the calculation of detector efficiency curves and particle self-shielding attenuation factors.²⁹ Table 2.8 lists correction factors for the two particle types from irradiation positions 50 and 181. These correction factors are primarily dependent on source-to-detector geometry, fuel kernel composition, burnup, and particle-coating design. Standard IMGA practice is that self-shielding attenuation and geometry correction factors are calculated and used in the quantitative analysis of the fission product gamma energy spectrum from each particle examined.

Table 2.8. Correction factors applicable to IMGA gamma spectrometry data on irradiated fuel from positions 50 and 148 of capsule HRB-15b

Isotope	Energy (keV)	Correction factors	
		Position 50 (Th,U)O ₂ -Triso	Position 148 UO ₂ -Triso
¹⁴⁴ Ce	133	0.7032	0.6910
¹⁰⁶ Ru	511	0.6969	0.6910
¹³⁴ Cs	604	0.6971	0.6910
¹⁰⁶ Ru	622	0.6970	0.6910
¹³⁷ Cs	661.6	0.6965	0.6910
⁹⁵ Zr	724	0.6954	0.6910
⁹⁵ Zr	756	0.6947	0.6910
¹³⁴ Cs	796	0.6938	0.6910

The neutronics data used in the new calculations are shown in Tables 2.9 and 2.10. Significant differences exist between the GA cross-section data set and the ORNL cross-section set, in particular the (n,γ) cross sections for ²³²Th and ²³⁸U and the (n,f) cross sections for ²³³U, ²³⁵U, and ²³⁹Pu. The most significant are the (n,γ) cross section for ²³⁸U and the (n,f) cross section for ²³⁵U. The ORNL value for ²³⁵U (n,f)

Table 2.9. ORNL and General Atomic (GA) integral flux data used in calculating fission product inventories for particles from capsule HRB-15b

Laboratory	Position	Integral flux [neutrons/(m ² ·s)]	
		HFIR cycles 1-3	HFIR cycles 4-8
ORNL	50	1.759×10^{19}	1.913×10^{19}
GA	50	1.956×10^{19}	2.089×10^{19}
ORNL	148	1.930×10^{19}	1.760×10^{19}
GA	148	2.001×10^{19}	1.851×10^{19}

Table 2.10. ORNL and General Atomic (GA) cross-section data used in calculating fission product inventories for particles from capsule HRB-15b

Isotope	Cross-section (b) ^a			
	(n,γ) Reaction		(n,f) Reaction	
	ORNL	GA	ORNL	GA
²³² Th	3.30	3.60	0.006	
²³³ Pa	32.2	30.2		
²³⁴ Pa	150.0	150.0	1500.0	1500.0
²³³ U	16.1	16.1	151.0	178.0
²³⁴ U	40.1	33.0		
²³⁵ U	27.9	30.5	144.0	111.0
²³⁶ U	10.2	10.2		
²³⁷ U	110.0	110.0		
²³⁸ U	5.908	4.40	0.023	0.023
²³⁷ Np	65.6	55.6		
²³⁸ Np	13.0	13.0	570.0	570.0
²³⁹ Np	8.3	8.3		
²³⁸ Pu	130.0	180.0		
²³⁹ Pu	142.0	140.0	281.0	299.0
²⁴⁰ Pu	287.0	280.0		
²⁴¹ Pu	109.0	109.0	329.0	329.0
²⁴² Pu	36.5	30.0		

^a1 b = 10⁻²⁸ m².

is about 30% higher than the GA value, and the ORNL value for ^{238}U (n, γ) is about 34% larger than the GA value. Table 2.9 shows the integral flux values used with the cross-section data sets in Table 2.10. Notable differences exist between the GA and ORNL flux values. For all the HFIR cycles and for each irradiation position, the ORNL flux data are lower (4.6 to 11.2%) than the GA flux data.

We compared the inventories of two fission products, ^{106}Ru and ^{137}Cs , as determined with the IMGA system with calculations from both sets of neutronics data from Table 2.10. The fission product ^{106}Ru was used as the stable (chemically) fission product instead of ^{95}Zr because of the much longer 269-d half-life of ^{106}Ru compared with 65 d for ^{95}Zr . Also the fission yields for ^{106}Ru are much higher for ^{239}Pu and ^{241}Pu than for ^{233}U and ^{235}U . These data allow a check of the reaction rates in the ^{238}U chain (^{238}U , ^{239}Pu , and ^{241}Pu), which are important in LEU fuels. The fission yields for ^{137}Cs are only slightly different for fissile uranium versus fissile plutonium fuels.

The results of the calculations with the neutronics data and the initial fissile and fertile fuel inventories are shown in Table 2.11 for the fission product ^{106}Ru and in Table 2.12 for ^{137}Cs . Using the ORNL data and with the (Th,U) O_2 kernel in position 50, the ratio of measured-to-calculated ^{106}Ru inventory is about 105%, and that for ^{137}Cs is 101%. These values compare with GA values of about 121% for ^{106}Ru and about 106% for ^{137}Cs . The UO_2 kernel in position 148 and the ORNL data yielded a ratio of measured-to-calculated inventory of 100% for both ^{106}Ru and ^{137}Cs . These values compare with values of about 122% for ^{106}Ru and of about 114% for ^{137}Cs for the GA data. These results clearly show that the ORNL neutronics data yield calculated fission product inventories in much better agreement with measured inventories determined with IMGA gamma spectrometry. The calculated fission product inventories with the GA data yielded consistently low values compared with measured inventories.

Gamma spectrometry measurements at GA have not been completed on all the particles investigated. However, for two particles of interest (No. 6 from irradiation position 50 and No. 19 from irradiation position 148) gamma spectrometry data have been completed and transmitted to ORNL. The

Table 2.11. Comparison of ORNL and General Atomic (GA) calculated and measured ^{106}Ru fission product inventories

Particle	^{106}Ru inventory (nmol)				
	IMGA data	Calculated ORNL data	Calculated GA data	IMGA-ORNL(%)	IMGA-GA (%)
<i>(Th,U)O₂ kernel, Triso coating, irradiation position 50, HRB-15b</i>					
1	2.598	2.514	2.183	103.3	119.0
2	2.654	2.411	2.094	110.1	126.8
3	2.705	2.507	2.177	107.9	124.2
4	2.557	2.613	2.270	97.9	112.6
5	2.632	2.560	2.223	102.8	118.4
6	2.743	2.595	2.253	105.7	121.7
7	2.561	2.383	2.069	107.5	123.8
8	2.657	2.500	2.172	106.3	122.4
9	2.580	2.554	2.218	101.0	116.3
10	2.552	2.472	2.147	103.2	118.8
15	2.697	2.439	2.119	110.6	127.3
19	2.612	2.560	2.223	102.0	117.5
21	2.626	2.399	2.084	109.4	126.0
Av				105.2	121.1
<i>UO₂ kernel, Triso coating, irradiation position 148, HRB-15b</i>					
1	2.566	2.690	2.213	95.4	116.0
3	2.700	2.547	2.095	106.0	128.9
4	2.690	2.384	1.962	112.8	137.1
5	2.642	2.754	2.266	95.9	116.6
7	2.726	2.301	1.894	118.5	143.9
8	2.629	2.607	2.145	100.8	122.6
9	2.557	2.945	2.424	86.8	105.5
10	2.542	2.495	2.054	101.9	123.8
11	2.582	2.625	2.160	98.4	119.5
13	2.490	2.361	1.944	105.5	128.1
17	2.710	2.680	2.205	101.1	122.9
18	2.587	2.439	2.007	106.1	128.9
19	2.547	2.852	2.346	89.3	108.6
21	2.539	2.675	2.202	94.9	115.3
22	2.615	2.945	2.428	88.8	107.7
Av				100.1	121.7

Table 2.12. Comparison of ORNL and General Atomic (GA) calculated and measured ^{137}Cs fission product inventories

Particle	^{137}Cs inventory (nmol)				
	IMGA data	Calculated ORNL data	Calculated GA data	IMGA-ORNL (%)	IMGA-GA (%)
<i>(Th,U)O₂ kernel, Triso coating, irradiation position 50, HRB-15b</i>					
1	10.928	10.827	10.359	100.9	105.5
2	10.945	10.380	9.930	105.4	110.2
3	10.898	10.794	10.325	101.0	105.5
4	10.656	11.252	10.764	94.7	99.0
5	10.906	11.024	10.548	98.9	103.4
6	11.293	11.240	10.752	100.5	105.0
7	10.923	10.257	9.814	106.5	111.3
8	10.851	10.767	10.301	100.8	105.3
9	10.794	10.998	10.523	98.1	102.6
10	10.686	10.648	10.022	100.4	106.6
15	11.067	10.505	10.051	105.3	110.1
19	10.871	11.024	10.548	98.6	103.1
21	10.791	10.332	9.885	104.4	109.2
Av				101.2	105.7
<i>UO₂ kernel, Triso coating, irradiation position 148, HRB-15b</i>					
1	10.076	10.302	9.110	97.8	110.6
3	10.137	9.753	8.625	103.9	117.5
4	10.278	9.133	8.077	112.4	127.3
5	10.059	10.548	9.329	95.4	107.8
7	10.093	8.813	7.793	114.5	129.5
8	10.042	9.982	8.828	100.6	113.7
9	9.909	11.282	9.977	87.7	99.2
10	9.822	9.557	8.451	102.7	116.2
11	10.186	10.050	8.888	101.3	114.5
13	9.854	9.044	7.998	108.8	123.1
17	10.251	10.262	9.075	99.8	112.8
18	9.916	9.343	8.263	106.0	119.9
19	9.913	10.921	9.660	90.6	102.4
21	9.918	10.244	9.059	96.8	109.4
22	10.017	11.282	9.977	88.7	100.3
Av				100.5	113.6

ORNL and GA fission product inventories for ^{134}Cs , ^{137}Cs , and ^{106}Ru are shown in Table 2.13. Also shown are the activity ratio calculations for $^{134}\text{Cs}/^{106}\text{Ru}$, $^{137}\text{Cs}/^{106}\text{Ru}$, and $^{134}\text{Cs}/^{137}\text{Cs}$ for these same particles. Significant differences exist between the absolute inventories from the two laboratories, the GA values being 8.8 to 17.5% lower. Comparison of the activity ratios shown indicates good agreement of the ORNL and GA data. Generally the GA-calculated ratios are slightly lower than the ORNL ratios: about 5% for $^{134}\text{Cs}/^{106}\text{Ru}$, about 1.5% for $^{137}\text{Cs}/^{106}\text{Ru}$, and less than 1% for $^{134}\text{Cs}/^{137}\text{Cs}$. The reason for the absolute inventory discrepancies between ORNL- and GA-derived data has not been resolved. Further gamma spectrometry measurements of GA will be performed on the remaining HRB-15b particles. On the completion of these measurements, a more thorough comparison will be made for the entire sets of GA and ORNL data.

Table 2.13. Tabulations of ORNL and General Atomic (GA) gamma-spectrometry data measured on the same particles from capsule HRB-15b

Isotope	Energy (keV)	Inventory		Activity ratio	Value	
		ORNL (10 ⁻⁴ Ci) ^a	GA (10 ⁻⁴ Ci) ^a		ORNL	GA
Irradiation position 50 — particle 6						
¹⁰⁶ Ru	511.8	9.696	8.531	¹³⁴ Cs/ ¹⁰⁶ Ru	0.319	0.304
¹³⁴ Cs	604.7	3.095	2.591	¹³⁷ Cs/ ¹⁰⁶ Ru	0.139	0.130
¹³⁷ Cs	661.6	1.346	1.111	¹³⁴ Cs/ ¹³⁷ Cs	2.299	2.332
Irradiation position 148 — particle 19						
¹⁰⁶ Ru	511.8	8.943	8.158	¹³⁴ Cs/ ¹⁰⁶ Ru	0.321	0.305
¹³⁴ Cs	604.7	2.874	2.486	¹³⁷ Cs/ ¹⁰⁶ Ru	0.132	0.13
¹³⁷ Cs	661.6	1.179	1.02	¹³⁴ Cs/ ¹³⁷ Cs	2.438	2.44

$$^a 1 \text{ Ci} = 3.7 \times 10^{10} \text{ Bq.}$$

The results of this investigation into resolution of HFIR Removable Beryllium Test Facility cross sections indicate that the ORNL-derived integrated flux and cross-section data provide calculated fission product inventories in much better agreement with observed inventories. We have therefore recommended that the ORNL neutronics data for cross sections described in Table 2.12 and integrated flux values described in ref. 32 (Table A.4) be used for future fuel depletion and thermal analysis calculation of HFIR experiments; GA has agreed to this action.³³

2.5.2 Postirradiation Gas Analyzer — M. J. Kania and J. C. McLaughlin

During the past reporting period, the PGA system was used to evaluate the fission gas content of irradiated particles from capsules HT-34, HRB-14, and HRB-15b. The results from HT-34 and HRB-14 are discussed in detail in Sect. 2.33 (PWS FD-12). Selected capsule HRB-15b particles fabricated by ORNL and irradiated in positions 19, 75, 79, 83, 89, 91, 95, 99, 109, 111, 119, 123, and 155 have been evaluated. Results of the measurements for the stable krypton isotopes ^{83}Kr , ^{84}Kr , and ^{86}Kr are presented in Table 2.14. Additional particles from positions 17, 25, 27, 85, 159, and 179 will be evaluated before the analysis of HRB-15b particle data is completed.

Several significant accomplishments were made with the PGA system and data analysis procedures this past year:

1. Fission gas content measurements on particles heated to elevated temperature (1100°C) were achieved for the high-temperature breaking chamber. The capability for fission gas release measurements exists, but CO and CO₂ measurement capabilities are not available because the software for data handling does not exist. This capability is not being used because of the release of contamination, especially radioactive cesium, to the time-of-flight mass spectrometric (TOFMS) flight tube located outside the PGA containment.

2. A new data analysis procedure⁶ was developed, which permits better mass peak centroid resolution and reproducibility from scan to scan. This procedure is applicable to both the stable krypton and xenon isotopes.

Table 2.14. Results of stable krypton fission gas content measurements on ORNL particles irradiated in capsule HRB-15b

Tray- particle	Batch	Coating type	Quantity of gas (nmol)			
			^{83}Kr	^{84}Kr	^{86}Kr	Kr
19-1	A-835	Triso	0.0058	0.0311	0.0455	0.0825
19-3	A-835	Triso	0.0092	0.0245	0.0401	0.0739
19-4	A-835	Triso	0.0135	0.0392	0.0589	0.1116
19-6	A-835	Triso	0.0086	0.0279	0.0456	0.0821
19-7	A-835	Triso	0.0071	0.0198	0.0290	0.0559
75-2	A-833	Triso	0.2201	1.0930	1.4051	2.7182
75-3	A-833	Triso	0.1999	0.9231	1.1783	2.3013
75-4	A-833	Triso	0.1654	0.7837	1.0019	1.9511
75-5	A-833	Triso	0.2427	1.2019	1.5498	2.9944
75-6	A-833	Triso	0.1995	0.9742	1.2445	2.4182
79-2	A-834	Triso	0.2582	1.3228	1.6706	3.2516
79-3	A-834	Triso	0.2175	1.1091	1.3838	2.7104
79-4	A-834	Triso	0.2483	1.2997	1.5950	3.1403
79-5	A-834	Triso	0.2446	1.2550	1.5657	3.0654
79-6	A-834	Triso	0.2126	1.0958	1.3520	2.6605
83-2	A-835	Triso	0.2010	0.9717	1.2456	2.4183
83-3	A-835	Triso	0.1877	0.9068	1.1451	2.2397
83-4	A-835	Triso	0.2119	1.0057	1.2919	2.5094
83-5	A-835	Triso	0.2468	1.2136	1.5456	3.0060
83-6	A-835	Triso	0.1990	0.9803	1.2572	2.4364
89-2	A-837	Triso	0.1976	0.9636	1.2607	2.4219
89-3	A-837	Triso	0.2678	1.3621	1.7563	3.3862
89-5	A-837	Triso	0.2622	1.3501	1.7601	3.3724
89-6	A-837	Triso	0.2397	1.2065	1.5558	3.0021
91-1	OR-2262	Biso	0.3206	1.5640	2.0528	3.9374
91-3	OR-2262	Biso	0.2610	1.3006	1.6727	3.2343
91-4	OR-2262	Biso	0.2620	1.2596	1.6691	3.1906
91-5	OR-2262	Biso	0.2528	1.2728	1.6782	3.2039
91-7	OR-2262	Biso	0.2766	1.3503	1.7563	3.3832
95-1	A-765	Biso	0.3088	1.5574	2.0212	3.8874
95-3	A-765	Biso	0.3078	1.5902	1.9681	3.8611
95-5	A-765	Biso	0.3341	1.7162	2.1322	4.1825
95-6	A-765	Biso	0.2091	1.0731	1.2775	2.5598
95-7	A-765	Biso	0.2776	1.4479	1.7707	3.4962

Table 2.14. (Continued)

Tray- particle	Batch	Coating type	Quantity of gas (nmol)			
			⁸³ Kr	⁸⁴ Kr	⁸⁶ Kr	Kr
99-2	A-786	Biso	0.2415	1.1816	1.5580	2.9811
99-3	A-786	Biso	0.2946	1.4741	1.9515	3.7203
99-4	A-786	Biso	0.2536	1.2520	1.6671	3.1727
99-5	A-786	Biso	0.2610	1.3061	1.6603	3.2274
99-6	A-786	Biso	0.2829	1.4093	1.8125	3.5047
109-1	J-482	Biso	0.2518	1.2675	1.5825	3.1018
109-3	J-482	Biso	0.2547	1.2829	1.6437	3.1813
109-4	J-482	Biso	0.3129	1.5862	2.0714	3.9705
109-5	J-482	Biso	0.2202	1.1003	1.3722	2.6927
109-7	J-482	Biso	0.3200	1.6123	2.0434	3.9757
111-2	J-489	Biso	0.2828	1.4220	1.7519	3.4567
111-3	J-489	Biso	0.3387	1.6925	2.1229	4.1541
111-4	J-489	Biso	0.2766	1.3780	1.6957	3.3503
111-6	J-489	Biso	0.2699	1.3704	1.6753	3.3156
119-1	J-649	Biso	0.3170	1.6335	2.0436	3.9941
119-3	J-649	Biso	0.2716	1.3202	1.6821	3.2704
119-4	J-649	Biso	0.3338	1.6800	2.1664	4.1802
119-5	J-649	Biso	0.2629	1.3058	1.6095	3.1782
119-6	J-649	Biso	0.2879	1.4415	1.7824	3.5119
119-7	J-649	Biso	0.3158	1.5799	1.9806	3.8763
123-2	J-651	Biso	0.2410	1.1240	1.4864	2.8515
123-3	J-651	Biso	0.2215	1.0583	1.3911	2.6710
123-4	J-651	Biso	0.2879	1.3527	1.7796	3.4202
123-5	J-651	Biso	0.2661	1.2519	1.6798	3.1978
123-6	J-651	Biso	0.2134	0.9723	1.2850	2.4706
155-1	A-765	Biso	0.0215	0.0826	0.1225	0.2261
155-3	A-765	Biso	0.0173	0.0627	0.0906	0.1705
155-4	A-765	Biso	0.0187	0.0692	0.0998	0.1875
155-5	A-765	Biso	0.0139	0.05118	0.0778	0.1429
155-6	A-765	Biso	0.0139	0.0575	0.0903	0.1618
155-7	A-765	Biso	0.0109	0.0455	0.0685	0.1250

3. Computational procedures were developed whereby PGA and IMGA data are coupled to generate a particle performance assessment based on both gaseous and metallic fission product retention.

A new turbomolecular pump has been ordered to replace the existing system ion pump. The new turbopump has almost a factor-of-10 increase in pumping speed, which is needed to handle the high gas load from the high-temperature chamber when in operation. Specifications have been written and the order placed for acquisition of a quadrupole mass spectrometer to be used for fission and reactive gas content measurements with the high-temperature chamber. The quadrupole head will be placed inside the glove box containment and will eliminate the contamination of the TOFMS flight tube as indicated in item 1 above. Thus, all major radioactive isotopes will be maintained within the glove-box containment.

2.5.3 X-Radiography Facility — M. J. Kania

The x-radiography facility provides the capability of preparing x-radiographs of irradiated coated particles for detecting heavy metal and fission product redistribution. This system is installed in the High Radiation Level Examination Laboratory (HRLEL) and is fully operational. During fiscal year 1981, the system has been evaluated and optimized with loose irradiated particles and small-diameter compacts composed of irradiated particles. In each case, the x-radiographs were of sufficient quality to detect heavy metal and fission product redistributions.

The facility has been used for determining fuel particle diameters of irradiated fuel particles from HRB-15b used in the resolution of the HFIR Removable Beryllium Test Facility cross section (Sect. 2.5.1.3). The system is now being used to evaluate the extent of thorium leached into buffer and low-temperature isotropic (LTI) coatings during chlorine leaching of irradiated particles from HRB-14.

2.6 SERVICE WORK FOR GENERAL ATOMIC COMPANY — M. J. Kania

Because of ORNL's unique examination capabilities in certain areas, service work is performed for GA on an irregular basis. Areas in which work is performed include IMGA, PGA, electron microprobe, x-radiography, SAXS, and TEM.

With the great interest in the microstructural properties of SiC, GA requested TEM, SAXS, and Raman scattering examinations on their candidate SiC-coated particles. The results of these investigations are reported in Sect. 2.3.2. General Atomic has also requested that, as part of the PIE on capsule HRB-15a, ORNL perform IMGA, PGA, x-radiography, metallography, and electron microprobe examinations on selected fuel specimens. The HRB-15a samples were received in August 1981, and the PIE work began in mid-December 1981. As results become available, they will be forwarded to GA.

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3. IRRADIATION SERVICES FOR GENERAL ATOMIC COMPANY

F. J. Homan, J. A. Conlin, and I. I. Siman-Tov

3.1 INTRODUCTION

Before 1981 irradiation capsules were roughly divided equally between ORNL and General Atomic Company (GA). The ORNL charter for fuel irradiations was the development and testing of candidate recycle fuels. With the demise of the HTGR Fuel Refabrication Program in the late 1970s, the ORNL objectives shifted to fresh (initial and makeup) fuel, specifically to providing irradiation data on oxide-based fuels for consideration in the selection of a reference fuel system for the low-enriched uranium (LEU) fuel cycle. The selection of the reference LEU fuel was scheduled for September 1981. Once the last capsules containing fuel for consideration in this selection were in the reactor, ORNL's role in the irradiation program changed from "experimenter" to provider of irradiation services for GA. Reporting on fuel irradiations before 1981 was accomplished in Chap. 2 (Fueled Graphite Development Program) but is now being reported in a separate chapter because of the above changes.

3.2 CAPSULE HRB-15a

The irradiation of HRB-15a was completed at the end of January 1981. The following sections update last year's report.¹

3.2.1 Irradiation History

Capsule HRB-15a completed irradiation on January 29, 1981. The capsule operated for 4171.68 h at the full reactor power of 100 MW. A maximum fast fluence of 6.893×10^{25} neutrons/m² ($E > 29$ fJ)_{HTGR} was achieved. This fluence is based on the flux data presented in Table 3.1.

Table 3.1. Total and fast fluxes characteristic for
High Flux Isotope Reactor removable beryllium
irradiation experiment facility^a

Position from top of core		Total flux ^b [neutrons/(m ² ·s)]	Fast flux ^b [neutrons/(m ² ·s)]
(mm)	(in.)		
0.00	0.0	0.71×10^{19}	1.55×10^{18}
25.40	1.0	0.86	1.94
50.80	2.0	1.06	2.36
76.20	3.0	1.34	2.78
101.60	4.0	1.64	3.21
127.00	5.0	1.83	3.59
152.40	6.0	2.08	3.96
177.80	7.0	2.22	4.22
203.20	8.0	2.36	4.42
228.60	9.0	2.44	4.53
254.00	10.0	2.46	4.59
279.40	11.0	2.44	4.53
304.80	12.0	2.35	4.37
330.20	13.0	2.23	4.17
355.60	14.0	2.08	3.89
381.00	15.0	1.87	3.54
406.40	16.0	1.63	3.14
431.80	17.0	1.39	2.68
457.20	18.0	1.15	2.33
482.60	19.0	0.95	1.89
508.00	20.0	0.74	1.58

^aThese fluxes were obtained from curves supplied by H. T. Kerr and J. F. Mincey, ORNL, in a letter to Dale Hill of General Atomic Company (date unknown).

^bThe values presented are averages taken between the fluxes given for the old and new beryllium reflectors ($E > 29$ fJ)_{HTGR}.

The following coefficients for a 7th-degree polynomial fit (obtained with a least-squares fit) were used to obtain total fluxes at positions other than those shown in Table 3.1:

$$\begin{aligned} a_0 &= 0.713373 \text{ E+19} \\ a_1 &= 5.46913 \text{ E+17} \\ a_2 &= 9.55162 \text{ E+17} \\ a_3 &= -1.94430 \text{ E+16} \\ a_4 &= 1.96274 \text{ E+16} \\ a_5 &= -1.19144 \text{ E+15} \\ a_6 &= 3.88940 \text{ E+15} \\ a_7 &= -5.11150 \text{ E+11} \end{aligned}$$

A 5th-degree polynomial with the following coefficients was used to obtain fast fluxes ($E > 29 \text{ fJ}$)_{HTGR} at positions other than those shown in Table 3.1:

$$\begin{aligned} a_0 &= 1.551390 \text{ E+18} \\ a_1 &= 0.357201 \text{ E+18} \\ a_2 &= 3.22098 \text{ E+16} \\ a_3 &= -4.89680 \text{ E+15} \\ a_4 &= 1.06604 \text{ E+14} \\ a_5 &= 6.60525 \text{ E+11} \end{aligned}$$

The position must be given in inches from the top of the HFIR core.

3.2.2 Irradiation Conditions and Measurements

Updated and completed data that summarize the fission gas release-to-birth-rate ratios (R/B) are presented in Table 3.2. These values were obtained with the CACA-2 code (ref. 2) and the ORNL fluxes and cross-section set are presented in Tables 3.1 and 3.3, respectively. The coefficients used for the time-dependent polynomial fit at each rod position were obtained from the data given in Table A.4 of ref. 3.

Updated and complete burnups and accumulated fast fluences for the fuel in each rod position are given in Table 3.4. These values are also based on the fluxes and cross-section set mentioned previously.

Table 3.2. Capsule HRB-15a release-to-birth-rate ratio (R/B) data

Sample	Date	Irradiation time (h)	R/B for each isotope				
			^{85m}Kr	^{87}Kr	^{88}Kr	^{135}Xe	^{133}Xe
1	7/28/80	62.12	6.96×10^{-6}	4.61×10^{-6}	3.78×10^{-6}	5.02×10^{-6}	2.02×10^{-5}
2	8/5/80	251.26	4.32	3.25	2.79	1.09	1.00
3	8/12/80	423.50	4.78	3.10	2.70	1.37	^a
4	8/25/80	709.32	5.69	3.71	3.39	1.56	3.60
5	8/29/80	802.06	1.69	1.10	1.01	1.25	1.65
6	9/2/80	899.09	16.39	10.66	9.63	9.24	9.44
7	9/16/80	1209.82	18.94	11.20	10.81	8.83	24.83
8	9/23/80	1370.28	20.01	13.88	12.19	14.60	11.90
9	10/10/80	1753.03	11.06	8.85	7.25	5.09	10.46
10	10/13/80	1826.54	19.76	13.75	12.79	14.69	30.40
11	10/17/80	1921.2	22.72	14.37	14.29	15.32	25.35
12	10/24/80	2090.04	16.21	11.25	10.18	7.75	7.52
13	10/31/80	2210.69	17.23	11.87	10.64	7.07	4.72
14	11/6/80	2359.70	21.33	14.37	12.85	7.78	8.24
15	11/14/80	2490.67	31.89	20.36	20.73	10.81	11.24
16	11/17/80	2562.67	34.60	21.54	19.55	13.27	1.33
17	11/26/80	2751.46	24.89	16.30	13.39	8.47	8.66
18	12/5/80	2967.96	54.47	31.97	35.14	20.50	42.13
19	12/11/80	3109.63	99.48	55.93	59.92	37.24	54.17
20	12/18/80	3191.16	159.73	105.03	97.05	55.71	56.71
21	12/22/80	3287.47	177.36	116.59	110.07	61.17	65.98
22	1/2/81	3555.14	268.77	173.44	181.76	118.75	175.89
23	1/9/81	3696.50	237.40	152.16	139.78	80.45	172.63
24	1/15/81	3836.66	292.43	182.82	188.24	101.89	231.13
25	1/26/81	4103.66	376.11	205.90	237.48	138.95	350.91

^aNot applicable.

Table 3.3. Cross section set characteristics for High Flux Isotope Reactor removable beryllium facility irradiation experiments (July 21, 1981)^a

Nuclide	Capture cross section	Fission cross section
	(b) ^b	(b) ^b
²³² Th	3.30	0.006
²³³ Pa	32.2	
²³⁴ Pa	150.0	1500.0
²³³ U	16.1	151.0
²³⁴ U	40.1	
²³⁵ U	27.9	144.0
²³⁶ U	10.2	
²³⁷ U	110.0	
²³⁸ U	5.908	0.023
²³⁷ Np	65.6	
²³⁸ Np	13.0	570.0
²³⁹ Np	8.3	
²³⁸ Pu	130.0	
²³⁹ Pu	142.0	281.0
²⁴⁰ Pu	287.0	
²⁴¹ Pu	109.0	329.0
²⁴² Pu	36.5	

^aThis cross-section set is referenced in a letter from F. J. Homan, ORNL, to O. M. Stansfield, General Atomic Company, San Diego, Calif., October 5, 1981.

^b₁ b = 10⁻²⁸ m².

Table 3.4. Calculated burnup and damage fluence for specimens in capsule HRB-15a at end of irradiation

Rod	Calculated burnup (% FIMA) ^a			Fluence (E > 29 fJ) (neutrons/m ²)
	²³⁵ U	²³⁸ U	²³² Th	
1	78.09	5.97	2.08	4.10 × 10 ²⁵
2	80.83	7.69	2.79	4.64
3	82.36	9.31	3.48	5.16
4	83.27	10.81	4.15	5.63
5	83.84	12.16	4.77	6.04
6	84.23	13.33	5.31	6.37
7	84.49	14.28	5.77	6.63
8	84.67	14.98	6.11	6.80
9	84.78	15.41	6.32	6.87
10	84.81	15.54	6.38	6.85
11	84.76	15.36	6.29	6.74
12	84.64	14.85	6.04	6.53
13	84.43	14.03	5.65	6.24
14	84.10	12.91	5.12	5.87
15	83.60	11.53	4.48	5.43
16	82.33	9.27	3.47	4.71
17	79.87	6.97	2.49	3.94
18	77.93	6.00	2.05	3.53

^aFIMA, fissions per initial (heavy) metal atom.

3.3 CAPSULE HRB-16

3.3.1 Capsule Assembly

The design, fabrication, and assembly of capsule HRB-16 proceeded according to plan. A new set of drawings (M-11782-ER-031E to 40E) was issued. These drawings were an updated version of the previous drawings and include the current equivalent material specifications and the current quality assurance requirements.

New graphite sleeves were fabricated by Continental Tool and Engineering, Inc., of Oak Ridge, Tennessee. Two sleeves were fabricated according to the specification of HRB-16. The third does not have the holes drilled from the bottom so that it can be completed to accommodate a future design. These sleeves were fabricated within specified tolerances and were delivered on time.

One of the thermocouples (TEs) (position 6) was replaced by a TE array tube (TCAT). The TCAT consisted of four 0.5-mm TEs assembled in a 1.72-mm stainless steel tube and swaged down to 1.6-mm OD. Figure 3.1 is

MS-81-0880-1

0.2 mm

Fig. 3.1. Thermocouple array tube (TCAT) containing seven Chromel vs Alumel thermocouples.

a photograph of a TCAT containing seven TEs in a tube (as an example). The TE positions in the TCAT corresponded to positions 1 through 4 of the ordinary TEs used in the capsule.

The fuel loading was performed on May 4, 1981, with the assistance of C. A. Young of GA. In these sleeves the centering pins were made of stainless steel instead of alumina. This substitution should be avoided if the sleeve undergoes bakeout at 1800°C before loading. If such a substitution is absolutely necessary, the bakeout temperature should not exceed 1000°C. The spare sleeve was used for the loading because the centering pins in the original sleeve were melted in the bakeout process. The spare sleeve did not undergo bakeout before loading; however, all the POCO graphite used in the fabrication of the sleeves was heat treated before machining.

3.3.2 Irradiation History

The assembly of HRB-16 was completed on schedule. The capsule was inserted in the High Flux Isotope Reactor (HFIR) RB-7 position and started full power operation on June 21, 1981, with cycle 210.

During execution of the insertion and startup of the capsule, the primary inlet gas line was purged while open, and a few drops of water came out of the inlet tube. After pressurizing the primary containment and checking for leaks, we concluded that the moisture came from the supply line tube. At startup time, however, capsule temperatures increased rapidly and climbed to above 600°C at 50-MW reactor power, indicating that moisture was probably in the capsule. Further investigation revealed the possibility of moisture transfer to the inlet line and primary containment from a newly calibrated pressure gage used in the pressure test on the capsule primary containment.

The capsule was purged with helium, and the temperature dropped from 600 to about 500°C. After 45 min, the helium flow was returned to the normal level and the temperature did not increase, indicating that the moisture had been driven out. No special activity was noted on the radiation monitors. We proceeded slowly to bring the reactor power up to 100 MW with no abnormal temperature excursion. A gas sample was analyzed by mass spectroscopy, and, although not completely conclusive, the moisture content was less than 3 ppm. Subsequent temperature readings indicated that the capsule stayed dry.

During cycle 210, the capsule was operated on 100% He flow for 15 d. In the first 5 d, the peak fuel centerline temperature in the central region started dropping from 1325 to 1205°C. However, temperature fluctuations of about 50°C in TE 1 (and to a lesser degree in TEs 2 and 3) required an extensive checkout of the system before taking action to increase the temperature in the fuel. We concluded that these fluctuations were probably from movement of the rods within the sleeve.

The capsule was removed and reinstalled in the RB-7 facility during the first shutdown for reactor operations work. The startup of cycle 211 on July 15 was smooth, with no unusual occurrences. During cycle 211 the capsule operated with a helium-neon mixture starting on the second day of operation. Thermocouples 1, 2, and 3 were much steadier for the first 10 d; however, occasional sudden changes started recurring. The radiation recorders did not show spiking or other unusual activity, and, viewing the relative changes in TE 1 versus TE 9 (the extra TE in the same axial position), we concluded that some movement in the rods was still the most probable cause of the changes. Plots showing TE behavior are presented in Fig. 3.2. These plots are generated by the Oak Ridge Research Reactor Data Acquisition and Control System (ORRDACS).

The capsule developed a leak of about 20.0 kPa/h (3.0 psi/h) from the secondary into the primary containment toward the end of the operation of cycle 211. The secondary containment pressure is normally held at 6.205 MPa (900 psi). The buffer volume of the system was doubled to 1000 cm³ to slow down the time between warning alarm conditions on the pressure indicators of the secondary containment. The leak did not change throughout the remainder of the irradiation period.

The capsule completed its scheduled eight-cycle irradiation on December 23, 1981. The capsule was removed from the RB-5 facility and stored in the HFIR pool to allow short-lived fission products to decay before removal to the hot cell for postirradiation examination (PIE). (The capsule had been moved from the RB-7 facility to the RB-5 facility at the cycle 5 shutdown, as discussed later.)

The operating history of capsule HRB-16 is summarized in Table 3.5. The total operating time was 4081.68 h at a full reactor power of 100 MW, resulting in a maximum fast fluence of 6.745×10^{25} neutrons/m² ($E > 29$ eV)_{HTGR}.

Fig. 3.2. Oak Ridge Research Reactor Data Acquisition and Control System (ORRDACS) recorded temperatures for 23.5 h, starting June 30, 1981, at 9:12 AM.

Table 3.5. Capsule HRB-16 reactor operating history

HFIR cycle	Cycle schedule				Irradiation time (h) ^a	
	Begin		End		In cycle	Total accumulated
	Date	Time	Date	Time		
210	6/21/81	05:06	7/12/81	18:33	516.24	516.24
211	7/15/81	21:11	8/6/81	12:20	517.92	1034.16
212	8/7/81	20:00	8/29/81	01:06	508.80	1542.96
213	8/30/81	15:17	9/20/81	09:42	498.24	2041.20
214 ^b	9/22/81	11:15	10/13/81	08:00	500.88	2542.08
215	10/17/81	17:31	11/7/81	23:03	511.20	3053.28
216	11/9/81	00:13	11/30/81	08:30	511.68	3564.96
217	12/1/81	21:55	12/23/81	11:23	516.72	4081.68

^aIrradiation time is given in equivalent hours at 100 MW.

^bThe reactor operated at reduced power (90, 95 MW) on several occasions because of weather conditions.

3.3.3 Operating Conditions and Measurements

Operation of the HRB-16 experiment included determination of a calculated average centerline temperature for every fuel rod position each cycle. An average temperature for all cycles at each reporting time was also calculated. Table 3.6 presents the computed averages in each cycle as well as the final centerline average temperatures for the entire experiment.

The computational model for the thermal analysis was expanded to include this averaging option. However, the assumption in the calculation is that temperatures remain constant during each day of the cycle. The code was also modified to include an assessment of the model agreement with experimental values by comparing measured and calculated graphite sleeve temperatures at the TE positions.

A crude attempt was made at correcting the centerline temperatures of the rods adjacent to the TEs. This correction was based on the assumption that the rise in the graphite temperature is directly proportional to the rise in the centerline temperature as long as the only variable considered uncertain is the fission heat rate in the fuel rods. The temperature drop ΔT from the centerline to the TE location consists of three parts:

Table 3.6 Average centerline temperatures of capsule HRB-16

Rod	Cycle (°C)								All cycles
	I	II	III ^a	IV	V	VI	VII	VIII	
1	969	1124	1155/1196	1165	1165	1153	1147	1120	1125
2 (TE 1, TE 9) ^b	1056	1098	1134/1181	1134	1163	1193	1229	1214	1153
3	1085	1191	1185/1234	1183	1180	1175	1178	1155	1167
4 (TE 2, TE 10)	1146	1173	1187/1237	1202	1221	1230	1240	1211	1201
5	1146	1214	1211/1258	1220	1224	1227	1237	1215	1211
6	1172	1225	1226/1271	1242	1249	1254	1267	1243	1235
7 (TE 3, TE 11)	1263	1237	1229/1274	1235	1243	1220	1229	1219	1235
8	1209	1240	1247/1291	1273	1284	1293	1308	1285	1267
9	1214	1240	1250/1294	1278	1290	1300	1317	1293	1274
10 (TE 4, 5, 6)	1215	1264	1272/1313	1285	1305	1297	1298	1277	1281
11	1229	1249	1258/1303	1287	1301	1312	1329	1305	1284
12	1232	1253	1259/1304	1285	1299	1311	1327	1303	1284
13 (TE 7)	1220	1250	1270/1311	1286	1310	1292	1302	1281	1277
14	1210	1247	1246/1293	1261	1275	1284	1298	1276	1262
15	1179	1229	1225/1273	1233	1245	1253	1265	1243	1237
16 (TE 8)	1208	1294	1319/1367	1308	1334	1333	1354	1326	1310
17	1176	1258	1244/1295	1233	1241	1244	1251	1229	1235
18	1208	1308	1286/1339	1263	1264	1259	1259	1238	1261

^aThe numbers to the right of the slash indicate the average centerline temperatures excluding the 100% helium days from the average. All other temperatures include the 100% helium data.

^bTE, thermocouple.

1. the temperature drop across a fuel rod,

$$\Delta T_1 = (\dot{q}_1 r_1^2) / 4k_1 ,$$

2. the temperature drop across an annular gas gap,

$$\Delta T_2 = [q_2 \ln(r_3/r_2)] / (2\pi k_2 L) ,$$

where q_2 is $\dot{q}_1 v_1$, and

3. the temperature drop through a graphite annulus (from the inner surface to the TE location),

$$\Delta T_3 = \dot{q}_3 / 2k_3 (r_3^2 - r_2^2) + [q_2 \ln(r_3/r_2)] / (2\pi k_3 L) ,$$

where

$$\Delta T = \Delta T_1 + \Delta T_2 + \Delta T_3 ,$$

$\dot{q}_1$ = volumetric heat generation rate in fuel,

q_2 = heat transfer rate through the gas gap,

$\dot{q}_3$ = volumetric heat generation rate in the graphite annulus,

r_i = radii shown in the following illustration,

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- k_i = thermal conductivity of the material in region with outer radius r_i ,
 v_1 = volume of fuel rod (of unit length),
 L = one unit length in a one-dimensional model.

The algorithm computing the corrected centerline temperature T_c^* for the rods at the thermocouple positions is

$$T_c^* = T_{\text{center}} + \frac{T_{\text{center}} - T_{\text{water}}}{T_{\text{graphite}} - T_{\text{water}}} (T_{\text{TE measured}} - T_{\text{graphite}}) .$$

Table 3.6 includes these corrected temperatures. Several additional factors can affect the TE readings:

1. The rods *cannot* be kept centered in the sleeve; therefore, the surrounding gas gap may not be uniform.
2. Heat generation gradients may occur in the rods and the sleeve because of self-shielding effects; hence, the circumferential orientation of the TE may make a difference in the temperature observed.
3. The TE may not be centered in the hole and may therefore either touch graphite or be surrounded by gas. This factor and the shielding effect may affect the actual temperature observed because a certain amount of nuclear heating exists in the TE itself.

Figures 3.3 through 3.8, representing the measured and calculated temperature in each TE location, indicate that the capsule was not achieving the desired temperatures at both ends. Because a mechanism was available in the RB-5 facility by which the capsule could be moved down 19.05 mm, an attempt to do so was made during the shutdown of cycle 5. Obstacles that arose and could not be overcome during the short shutdown period forced us to continue operating in the RB-5 facility at the previous elevation. No changes in operating conditions were observed because of the move to the RB-5 facility from the RB-7 position.

Data that summarize the ratios of R/B for the complete irradiation period are presented in Table 3.7. Calculated burnups and accumulated fast fluences are given in Table 3.8. These are based on the fluxes and the cross-section set presented in Tables 3.1 and 3.3, respectively.

Fig. 3.3. Calculated and measured graphite sleeve temperatures for position adjacent to rod 2 (thermocouples 1 and 9).

Fig. 3.4. Calculated and measured graphite sleeve temperatures for position adjacent to rod 4 (thermocouples 2 and 10).

Fig. 3.5. Calculated and measured graphite sleeve temperatures for position adjacent to rod 7 (thermocouples 3 and 11).

Fig. 3.6. Calculated and measured graphite sleeve temperatures for position adjacent to rod 10 (thermocouples 4, 5, and 6).

Fig. 3.7. Calculated and measured graphite sleeve temperatures for position adjacent to rod 13 (thermocouple 7).

Fig. 3.8. Calculated and measured graphite sleeve temperatures for position adjacent to rod 16 (thermocouple 8).

Table 3.7. Capsule HRB-16 release-to-birth-rate ratio (R/B) data

Sample	Date	Irradiation time (h)	R/B for each isotope				
			^{85m}Kr	^{87}Kr	^{88}Kr	^{135}Xe	^{133}Xe
1	6/26/81	125.3	2.44×10^{-5}	1.33×10^{-5}	1.02×10^{-5}	2.11×10^{-5}	1.07×10^{-4}
2	7/7/81	391.3	0.10	0.08	0.06	0.07	0.04
3	7/21/81	650.7	0.72	0.48	0.44	0.36	0.31
4	7/27/81	795.0	0.84	0.50	0.49	0.37	0.79
5	7/31/81	890.9	0.81	0.48	0.47	0.35	0.92
6	8/5/81	1011.6	1.08	0.65	0.52	0.56	0.73
7	8/12/81	1150.0	0.69	0.46	0.42	0.37	0.42
8	8/17/81	1266.2	1.26	0.84	0.77	0.58	0.96
9	8/21/81	1362.5	1.19	0.78	0.73	0.60	1.56
10	8/26/81	1482.2	0.82	0.52	0.51	0.27	0.41
11	9/3/81	1637.0	0.78	0.54	0.48	0.31	0.24
12	9/9/81	1781.8	1.00	0.69	0.64	0.36	0.52
13	9/18/81	1996.8	1.40	0.96	0.88	0.54	1.01
14	9/24/81	2094.7	1.05	0.71	0.66	0.29	0.22
15	10/1/81	2261.3	1.61	1.11	1.08	0.40	0.48
16	10/9/81	2449.6	1.83	1.25	1.14	0.44	0.60
17	10/20/81	2607.4	3.75	2.41	2.19	1.10	0.98
18	10/27/81	2776.0	5.00	2.92	3.23	1.16	1.57
19	11/2/81	2919.7	5.08	2.95	2.86	1.07	1.05
20	11/6/81	3015.8	5.12	3.07	2.84	1.37	1.06
21	11/13/81	3148.7	12.17	7.44	7.40	4.70	7.43
22	11/19/81	3296.7	15.10	8.76	9.11	4.86	10.02
23	11/24/81	3413.6	19.76	11.08	11.68	7.00	18.68
24	12/7/81	3675.4	19.61	10.75	10.10	6.21	11.42
25	12/11/81	3771.0	20.37	10.79	10.22	6.17	15.85
26	12/17/81	3914.1	20.84	12.78	13.58	7.33	19.58
27	12/22/81	4037.7	3.81	12.44	8.51	6.24	0.09

Table 3.8. Calculated burnup and damage
fluence for specimens in capsule HRB-16
at end of irradiation^a

Rod	Calculated burnup (% FIMA) ^b			Fluence (neutrons/m ²) (<i>E</i> > 20 fJ)
	²³² Th	²³⁸ U	²³⁵ U	
1	1.1	68.3	3.40	3.31 × 10 ²⁵
2	1.7	76.2	5.2	3.91
3	2.3	79.5	6.7	4.44
4	3.0	81.5	8.3	5.01
5	3.5	82.6	9.7	5.47
6	4.1	83.4	11.0	5.92
7	4.5	83.8	12.0	6.25
8	5.0	84.1	13.0	6.52
9	5.2	84.3	13.5	6.67
10	5.4	84.4	13.8	6.73
11	5.4	84.4	13.8	6.69
12	5.2	84.3	13.4	6.54
13	4.9	84.1	12.8	6.32
14	4.4	83.7	11.8	5.97
15	3.9	83.2	10.6	5.58
16	3.2	82.2	9.0	5.07
17	2.6	80.7	7.6	4.58
18	2.3	79.4	6.6	3.99

^aTotal hours, 4081.68 h at 100 MW.

^bFIMA, fission per initial (heavy) metal
atom.

The actual fuel loadings in the rods were not supplied to ORNL by GA, who directed us to use the design loadings in the thermal analysis and the fission product concentration calculation. The accuracy of both fission heat generation rates and release rates depends on this information.

3.4 REFERENCES

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4. PRESTRESSED CONCRETE PRESSURE VESSEL DEVELOPMENT

D. J. Naus

4.1 INTRODUCTION

The Prestressed Concrete Reactor Vessel (PCRVR) research and development program consists of generic studies designed to provide technical support for ongoing PCRVR activities, to contribute to the technological data base, and to provide independent review and evaluation of the relevant technology. These studies are identified through direct communication with PCRVR designers, participation in American Concrete Institute-American Society of Mechanical Engineers (ACI-ASME) committee activities, literature reviews, and communications with researchers in other countries. The program involves four basic interrelated activities: (1) assessments, (2) analysis development, (3) materials studies, and (4) model testing. During this reporting period studies were conducted in six task areas: (1) analysis methods development; (2) concrete properties in nuclear environment; (3) tendon and reinforcing steel materials; (4) instrumentation evaluation and development; (5) structural model tests; and (6) liner, cooling, and insulation systems.

4.2 ANALYSIS METHODS — J. R. Dougan, Z. P. Bazant,* and E. C. Rossow*

The objective of the PCRVR analytical effort is to develop techniques that can be incorporated into existing modern finite-element computer programs to improve their accuracy and efficiency. We are currently investigating finite-element computation of creep effects in concrete structures, an embedded rebar modeling technique, and a program for calculating equivalent nodal forces resulting from pressure loadings.

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4.2.1 Finite-Element Computation of Creep Effects

During this reporting period the subcontract with Northwestern University to develop a set of computer subroutines for creep analysis of concrete structures was completed. A summary of the program developed follows. Bazant and Rossow may be consulted for more details and for results obtained from several test problems: an axially loaded thin cylinder, a radially loaded thin cylinder, a standard concrete test cylinder, and a single-cavity PCRV.¹

To evaluate the full benefit of the safety and reliability of concrete containment structures, not only the strength effects of the concrete and reinforcing materials but also the actual inelastic behavior of these materials must be considered. In concrete vessels acting as primary or secondary containments, creep of the concrete is the major cause of inelastic behavior during the vessel's service life. During the past decade, knowledge about this phenomenon has advanced tremendously, and methods that allow an accurate and realistic analysis of the service conditions have been developed.

A general-purpose finite-element code for the creep analysis of nuclear concrete structures was recently developed by Anderson and coworkers.²⁻⁴ This program utilizes a rate-type creep law proposed in 1971 by Bazant,⁵ which is based on expanding the compliance function for aging concrete (also called creep function) into a series of real exponentials, called the Dirichlet series. Subsequently, however, this formulation was found to have some theoretical disadvantages from the thermodynamic restrictions resulting from the aging of concrete.⁶ Although the general form of the creep constitutive relation is thermodynamically correct, some parameters of the model can be negative when the experimental creep data for concrete are fitted, even though according to thermodynamic limitations they should be positive. This situation is not necessarily incorrect, because these are internal variables, whereas the thermodynamic restrictions must be satisfied only in total by the ensemble of the internal variables. However, such a feature is a matter of concern; therefore, use of a constitutive relation whereby the fulfillment of the thermodynamic restrictions is assured appears to be

safer. This method seems to hold for an analogous rate type formulation based on a Dirichlet series expansion of the relaxation function rather than on the compliance function and modeled by an aging Maxwell chain model.⁷⁻¹⁰ This formulation has been used in extending the finite-element code NONSAP* used in this investigation.

4.2.1.1 Constitutive Relation for Creep

The creep law is assumed to be linear, that is, to follow the principle of superposition.⁷ The linearity assumption is acceptable for concrete with stresses of less than about one-half the strength only, which is typical of nuclear vessels and containments under service conditions. Even within this range, however, significant deviations from linearity are observed on unloading and in a regime with decreasing strains. In such cases, the program cannot yield good results. Another phenomenon that spoils the linearity, even within the service stress range, is the simultaneous drying; however, this drying is not severe for nuclear concrete structures because they are massive and are also sealed by the steel liner on the interior face.

The aging of concrete, particularly the gradual change of creep properties as a function of age, is considered. This phenomenon causes the parameters of the Dirichlet series expansion of the relaxation function that represent the elastic moduli of the Maxwell chain model to depend on the age of the concrete.

The effect of temperature variations (limited to temperatures under 80°C) on the creep behavior is also considered. This effect includes both (1) the increase of creep rate due to heating and (2) the acceleration of aging as modeled by the equivalent hydration period (maturity).^{7,8} Both these effects are modeled by use of the activation energy concept, with different activation energies for the creep rate and for the process of aging (hydration).

The creep formulation used in the program cannot accommodate the general effects of time-variable pore humidity in concrete. These effects bring about a major complication of the constitutive relation⁷ and require

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the use of additional variables to characterize partial stresses of solid and water components; the effects also require a good model for tensile cracking from hygral stresses. Nevertheless, omission of moisture effects is admissible for massive structures such as nuclear vessels and containments because of their thickness and because the use of the steel liner of the interior face does not permit significant drying to occur except in a narrow layer near the exterior surface.

However, an approximate consideration of the humidity effect, which does not cause a major complication of the mathematical formulation, is possible with our program. The user is allowed to specify the mean ambient humidity (rather than the pore humidity in concrete or a time-variable quantity) for each finite element. The coefficients of the creep law are then adjusted to give a creep function that corresponds to concrete cylinders exposed to this environmental humidity, which is kept constant for the duration of the creep test. The approximate practical creep prediction model given in parts I and III of ref. 3 is used for this purpose. As for the definition of shrinkage, we can either use the formulas from ref. 3 or specify a set of experimentally observed shrinkage values. The foregoing consideration of humidity effects is very crude and yields realistic values for only the overall internal force results caused by humidity effects within the cross section of the structure. The method does not permit evaluating the distribution of the stresses produced by humidity effects throughout the cross section.

4.2.1.2 Numerical Algorithm

As clearly demonstrated earlier, a rate-type creep law allows a much more efficient finite-element analysis than does the integral-type creep law based directly on the principle of superposition.⁷ As discussed above, our program uses a rate-type creep law based on expanding the relaxation function of concrete into a series of real exponentials, called the Dirichlet series. This creep formulation may be visualized by the Maxwell chain model, the elastic moduli of which depend on the age of concrete, more precisely on the equivalent hydration period. This creep formulation is completely specified by a set of relaxation times, which are automatically selected in the program according to the rules given by

Bazant and Wu^{7,9} and the age-dependent elastic moduli of the Maxwell chain. These moduli are automatically generated from the relaxation function, which itself is automatically determined from the compliance function (creep function). The program also makes a check by reproducing the creep curve on the basis of the Maxwell chain model as presented by Bazant and Aschari.¹⁰

The rate-type creep law consists of a set of first-order differential equations for the partial stresses of the Maxwell chain and for the total strain of the Maxwell chain.^{7,10} It has been shown before that the usual step-by-step methods for ordinary differential equations cannot be applied to this system of equations. This problem is completely avoided by the so-called exponential algorithm, which is based on an exact solution of the differential equations for the Maxwell chain under the assumption that all parameters are constant within the time step.⁷⁻⁹ This exponential algorithm was proven and was demonstrated to be unconditionally stable and highly accurate even for very long time steps. This method allows for roughly increasing the time step in a geometric progression and keeping it approximately constant in the log-time scale.

4.2.1.3 Flow Chart of the Program

Rather than exhibit the flow chart graphically, we describe it as follows:

The program implements a step-by-step stable integration procedure combined with iterations in each time step. A typical time step consists of a two-cycle computation. In the first cycle, estimates of the incremental quantities for the step are computed by use of the material properties at the beginning of the time step. In the second cycle, the material properties are evaluated for the midstep on the basis of results of the previous cycle. The flow of the program is given below (the subroutines involved in each step of the program are mentioned in parentheses).

1. Read and check the input data (INPUT, MATPAR).
2. Compute the material parameters for the Maxwell chain model (MATPAR, PRINT1).
3. Initialize and set the step number to zero (MAINC).

4. Increment the step number and check whether or not the final step is completed. If it is completed, the final output is printed and the program ends (MAINC, PRINT2).

5. For the current time step, the age of the concrete, applied external loads, and temperature are determined, and the iteration parameter is set to zero (MAINC, CONSTI).

6. The iteration parameter is incremented, and the material properties are established on the basis of the current age of concrete (CONSTI, TEMHUM, DIFFUS, EMUF).

7. The instantaneous stiffness matrix is established for each element (ASREEK).

8. The incremental inelastic strains and the corresponding load vector are evaluated for each element (ASREEO).

9. The structural linear equation system for the nodal displacement increments and the load vector are assembled (MERGE).

10. The prescribed boundary constraints are imposed, and the equation system is solved for the incremental displacements (BOCOND, BANSOL).

11. If the iteration parameter = 1, the midstep stresses and strains are computed and the program returns to step 6 (RETRCK, STRESS, ASREEP). If the iteration parameter = 2, the program proceeds to step 12.

12. The incremental strains and stresses are computed; the total stresses, strains, and displacements are computed; and selective output is printed. The program then returns to step 4 (RETRCK, STRESS, PRINT2).

4.2.1.4 Determination of Creep Properties

The creep properties are determined by the subroutine (MATPAR), which calls for subroutines as specified below.

1. Read the number of decades in log-time scale to be considered, the number of steps per decade, the number of elements in Maxwell chain, the time for the start of the first time step, and the first relaxation time.

2. Read the input option number and the corresponding material data from which the characterization of the compliance function is developed. If the drying is specified, also read the characteristics for shrinkage and drying creep.

3. Compute the discrete values of the relaxation function for various strain deviations and various ages at the start of relaxation (this consists of direct numerical solution of a linear Volterra integral equation) (RELAX).

4. Compute the discrete values of the Maxwell chain moduli at all discrete ages (MAXWL1).

5. As a check, compute discrete values of the compliance function from the discrete values of Maxwell chain moduli for various ages (CRCURV). Print the resulting values of the compliance function and their deviations from the initially specified values of the compliance function. Also calculate and print the coefficient of variation of these deviations.

While the relaxation function is being determined, a subroutine for the compliance function is repeatedly called. This subroutine has three options to be specified by the user:

1. The compliance function is evaluated by interpolation from a given array of discrete values. The interpolation is linear in the logarithm of creep duration and in the logarithm of age at loading. For durations and ages falling outside the range, extrapolation is automatically used.

2. The compliance function is evaluated from a formula corresponding to the double-power law.³

3. If drying is specified, the compliance function is evaluated from a formula consisting of the double-power law augmented by a drying term.³

The ultimate result obtained in the subroutine MATPAR is an array of the discrete variables of the moduli of the Maxwell chain. For any specified age, the values of the Maxwell chain moduli are computed from the discrete values (in subroutine function EMUF) by a linear interpolation in the logarithm of the age of concrete. For arguments falling outside the time range, a linear extrapolation from the extreme values is used.

4.2.1.5 Input Options for Material Characterization

For convenience of the user, a number of input options are provided in the subroutine MATPAR:

1. The compliance function is specified as an array of discrete values for various load durations and ages at loading. No drying is considered.

2. Drying is considered, and the mean compliance function for the cross section is specified as in option 1. The values of the mean shrinkage strain of the cross section are also specified for various durations of drying and for various ages at the start of drying.

3. The compliance function is given by the double-power law, for which all of its five parameters are read. No drying is considered.

4. Same as option 3, but all double-power law parameters except the 28-d elastic modulus are generated from the given strength and composition parameters of concrete (the mix ratio of water, cement, sand, and gravel; cement type; and unit weight).

5. Same as option 4 except that the 28-d elastic modulus is also predicted from the strength and composition parameters.

6. Drying is specified, and the compliance function is defined by the double-power law augmented by the term for the creep increase due to drying.³ A formula for the shrinkage strain development (according to ref. 3) is also specified, and all parameters in these formulas are read.

7. Same as option 6, but all material parameters except the 28-d elastic modulus and the final shrinkage strain are predicted from strength and composition of concrete (and also the given ambient humidity).

8. Same as 6, but all parameters except the 28-d elastic modulus are predicted from the strength and composition of concrete.

9. Same as option 6, but all parameters are predicted from the strength and composition of concrete.

10. Two of the five double-power-law parameters (named E_0 and ϕ_1 ; see ref. 3) are determined to obtain the best fit (in the least-squares sense) of the given array of discrete values of the compliance function at various load durations and ages. The remaining three double-power-law parameters are given. This option is used when the given array of discrete values for the compliance function is of limited range in time or age. No drying is considered in this option. As a check, the coefficient of variation for the deviations of the formula from the given array of values for the compliance function is automatically computed and printed.

11. Same as option 10, but the remaining three double-power-law parameters are not specified; they are predicted from given strength and composition of concrete.

12. Same as option 10, but drying is considered.

13. Same as option 11, but drying is considered.

The user selects his input option according to the amount of information available to him before the analysis. If sufficient test data have been obtained, as is frequently done for nuclear concrete structures, the measured discrete values of the compliance function should be used (if the range of these variables is too limited, it is preferable to approximate these values by the double-power laws). If no experimental information is available, the double-power-law parameters are predicted from composition. However, if there is some information on the double-power-law parameters for a similar concrete, an adjustment of some of its parameters is appropriate.¹¹ Moreover, if the short-time deformation (elastic modulus) is measured, it should also be used to improve the parameters of the double-power law. The choice of the proper input option has a great effect on the accuracy in representing the concrete properties.

4.2.1.6 Conclusions

The computer program developed allows a rather efficient, versatile, and realistic analysis of the creep effects in nuclear concrete structures under long-time loading conditions and prescribed temperature variations even though it does not consider significant drying effects.

4.2.2 Embedded-Bar Modeling Technique

In conducting finite-element computer code analyses of PCRVs and related components, correct representation of individual reinforcing bars is virtually impossible with the truss elements included in most of these codes. The usual practice is to model the vessel or component with relatively large isoparametric elements, each of which may contain portions of several reinforcing bars or rebars. The truss element generally incorporated into most finite-element codes permits the rebar to lie only along the edges of the continuum elements. As a result, most rebars are either combined to form single bars or entirely omitted from the analysis.¹²

Furthermore, inclusion of the rebars in the analysis generally dictates the shape of the resultant finite-element mesh, which consequently contains elements of a less-than-desirable shape.^{13,14}

To alleviate this difficulty, we developed a procedure that allows arbitrarily oriented rebars to be embedded in isoparametric continuum elements. The necessity of so defining a mesh that the element edges and rebars coincide is eliminated so that curved bars can be used to facilitate the modeling of complex arrays of rebars such as those around the penetrations in PCRVs.

The embedded-bar concept was incorporated into the most recent ORNL two- and three-dimensional (3-D) versions of the ADINA¹⁵ finite-element code and was validated for simple test cases. (Unfortunately, the validation of the concept for these cases was not documented.) To verify the validity of the concept further, we conducted analyses of the end anchor reinforcement test specimens fabricated by Wiss, Janney, Elstner and Associates.¹⁶ These test specimens were intended to simulate the anchorage zone of posttensioned tendons in secondary containment vessels of nuclear power plants. The specimens had a large amount of reinforcing, most of which was monitored with strain gages and therefore should provide a good basis for verification of the embedded-bar concept.

4.2.2.1 Embedded-Bar Concept

An algorithm was developed to calculate the stiffness for isoparametric elements containing embedded steel reinforcing bars having arbitrary orientation. We developed the algorithm by introducing positional nodes to define the location of the rebar and an additional base coordinate λ . The mapping from the dual-basis system (Fig. 4.1) is defined by

$$P(\xi, \eta) = \sum N_i(\xi, \eta) P_i \quad (4.1)$$

and

$$\bar{P}(\lambda) = \sum \bar{N}_i(\lambda) \bar{P}_i, \quad (4.2)$$

where P_i is the position vector of the i th node of the element, P is the position vector of the point in the global space corresponding to the

basis coordinates (ξ, η) , and $N_i(\xi, \eta)$ are the shape functions that define the mapping. The quantities $\bar{P}(\lambda)$, $N_i(\lambda)$, and P_i are corresponding quantities defining the mapping of the rebar.

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Fig. 4.1. Dual mapping of solid element containing embedded rebar.

The total stiffness for the element is given by

$$[K_{ij}] = [k_{ij}]_C + [k_{ij}]_B, \quad (4.3)$$

where

$$[k_{ij}]_C = \int_{\text{element}} [\mathbf{B}]^T [\mathbf{D}] [\mathbf{B}] dV, \quad (4.4)$$

and

$$[K_{ij}]_B = \int_{\text{bar}} [\bar{\mathbf{B}}]^T \bar{\mathbf{D}} [\bar{\mathbf{B}}] d\lambda. \quad (4.5)$$

Superscript T refers to the transpose of $[\mathbf{B}]$ and $[\bar{\mathbf{B}}]$. The matrix $[\mathbf{B}]$ is defined in the usual manner as the matrix that relates element strains $\{\epsilon\}$ to nodal displacements $\{\delta_i\}$; that is,

$$\{\epsilon\} = [B] \{\delta_i\} , \quad (4.6)$$

and is obtained by assuming that the displacement field is defined by

$$\delta(\xi, \eta) = \sum N_i(\xi, \eta) \delta_i . \quad (4.7)$$

The $[D]$ matrix relates stress to strain; that is,

$$\{\sigma\} = [D] \{\epsilon\} . \quad (4.8)$$

The row matrix $[\bar{B}]$ relates the axial strain in the bar ϵ_d to the nodal displacements and is determined by assuming compatibility of deformation between the concrete and the rebar. The quantity $\bar{D}$ defines the relation between axial strain and axial force in the rebar.

The matrix $[k_{ij}]_C$ is calculated in the usual manner by use of numerical integration by summing the corresponding weights and function values at the integration points of the (ξ, η) coordinate system; that is,

$$[k_{ij}]_C = \int_{\text{element}} [B]^T [D] [B] dV \approx \sum w_{if}(\xi_i, \eta_i) . \quad (4.9)$$

Similarly, the matrix $[k_{ij}]_B$ is calculated by use of numerical integration by summing the corresponding weights and function values in the λ coordinate system; that is,

$$[k_{uk}]_B = \int_{\text{bar}} [\bar{B}]^T \bar{D} [\bar{B}] dl \approx \sum h_{ig}(\lambda_i) . \quad (4.10)$$

The difficulty that arises is that the $[\bar{B}]$ matrix is defined in terms of the basis coordinates (ξ, η) so that the values of the variables corresponding to the integration points λ_i cannot be determined directly. The problem was overcome by mapping the integration points into the global system with Eq. (4.2) and iteratively solving Eq. (4.1) for the corresponding values of ξ and η .

This procedure has been implemented and validated for certain test cases. Although the algorithm is somewhat complex, its execution times

are reasonable, and it offers the advantage that the user can develop the finite-element discretation without restrictions on element orientations or boundary positions relative to rebar positions. This advantage is particularly desirable and almost necessary in modeling the haunch and steam generator cavity regions of a multicavity PCRV.

4.2.2.2 Anchorage Zone Analyses

Test specimens

To verify the performance of end anchor zone reinforcements, Wiss, Janney, Elstner and Associates fabricated two anchorage blocks with different reinforcing ratios.¹⁶ The specimen design and testing procedure was prepared by Bechtel, the tendon hardware was furnished by INRYCO (formerly Inland-Ryerson Construction Products Company), and the specimens were tested at the University of Illinois. Strain gages were attached to selected rebars to monitor the strain at various locations during loading.

Both specimens had the same overall dimensions: 1.2 by 1.2 by 0.8 m; a central steel trumpet having 0.0032-m wall thickness by 0.18-m OD; a 0.52- by 0.52- by 0.095-m bearing plate; and a 0.3- by 0.3- by 0.025-m blockout at the bottom. Block 1 had 0.0043 m² of reinforcing steel, and block 2 had 0.0082 m². The steel was placed in each of the transverse directions, normal to the load axis. Figures 4.2 and 4.3 show the layouts of the rebars in blocks 1 and 2, respectively. Also, thin steel fins were incorporated as crack starters in the reduced cross-sectional area regions. The blocks were loaded in the sequence 7.1, 0, 7.1, 6.5, 7.1, 8.0, 8.9, 10.0, 11.1, 12.2, and 13.3 MN and unloaded in the reverse sequence. More detailed information about the test specimens and procedures has been published.¹⁶

Analytical models

The finite-element code ADINA,¹⁵ modified to include the embedded-bar concept, was used to conduct the analyses. This code contains material models to represent both the concrete and the steel portions of the blocks. A 3-D representation of the blocks was required, but because of symmetry only one-fourth of the block (viewed along the load axis) was modeled. The steel trumpet was assumed to have a negligible effect on the analysis and was ignored, as were the crack starter fins.

Fig. 4.2. Block 1 reinforcement.

Fig. 4.3. Block 2 reinforcement.

We used 84 3-D continuum elements to model the concrete in block 1, 5 elements to model the bearing pad, and 88 embedded bars to model the 12 rebars in block 1. Block 2 was identical to block 1 except that 136 embedded-bar elements were needed to model the 19 rebars contained. Figure 4.4 shows the mesh layout used for blocks 1 and 2.

Concrete compressive strengths used in the analyses were 31.85 MPa for block 1 and 30.47 MPa for block 2. Both blocks had a concrete modulus of elasticity of 22.89 GPa. The allowable tensile strength of the concrete was taken as 3.19 MPa for block 1 and 3.05 MPa for block 2. (The tensile strength of a portion of both blocks was revised at a later point because of a solution stability problem. This problem, along with the revised values, is discussed in the next section.)

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Fig. 4.4. Mesh layout of blocks 1 and 2.

The bearing plate was defined as a linear elastic steel with a modulus of elasticity of 200 GPa. The rebars were analyzed as materially nonlinear with a modulus of elasticity of 200 GPa, a yield stress of 414 MPa, and a hardening modulus of 2 GPa.

Analysis methods

The concentrated loading actually applied to the test specimen was interpreted for the analysis as a pressure load acting over the bearing plate. Therefore, the load was applied through the nodal points of the bearing plate and corresponds to a concentrated load divided by the bearing plate area to give a uniform pressure. Because only one-fourth of each block was analyzed, only one-fourth of the total load was applied.

A major problem related to the cracking of the concrete occurred during the analysis. The analysis was stopping prematurely because of a lack of equilibrium between internal and external forces. Subsequent investigation of the state of the materials just before the stopping point revealed significant cracking of the concrete on the top surface. It was believed that this severe cracking was responsible for the premature stoppage of the analysis. To verify that the cracking was in fact responsible, the allowable tensile stress of the top layer of concrete was increased arbitrarily to 8.3 MPa for both blocks. The subsequent analysis then proceeded as expected. This increase in strength of the top layer was felt to have a negligible, if any, effect on the behavior of the remainder of the specimen; however, another problem arose as the analysis progressed. The original intent of the analysis was to follow the loading cycle specified by Franklin and Johnson¹⁶ as closely as possible. Unfortunately, after passing the maximum load and while on the unloading portion of the load cycle, the analysis stopped. This condition was clearly not one of structural failure because the specimens had been subjected to the maximum load without failure. The cause of this problem has not been isolated, although it may be due in part to a problem with cracked concrete. (An interesting point was that the rebar strains continued to increase, even during unloading.) A decision was made to analyze the blocks only under increasing load up to the maximum. The loading sequence was consequently revised to 3.6, 7.1, 8.9, 9.8, 10.7, 11.6, 12.5, and 13.3 MN for the total block.

With the change in the tensile strength and the revised load curve, the analysis was conducted without problem. Although these changes made the analysis less representative of the actual tests, the principal objective of verifying the embedded-bar concept was still achieved.

4.2.2.3 Conclusion

Only general agreement was obtained between the experimentally measured strain values and those determined analytically. Such a result is to be expected, however, because, given the loading and unloading cycle of the experiment and the lack of an unloading cycle in the analysis, some disagreement was bound to occur. Also, the concrete model has a history of being much stiffer than the actual concrete being modeled.¹³

The principal advantage of the embedded-bar concept is the removal of the criterion that element boundaries must coincide with the locations of the reinforcing steel. The principal disadvantage of the concept is that the behavior of an embedded bar depends directly on the behavior of its parent element. Simply stated, the behavior of an embedded bar is no more accurate than the behavior of the material in which it is embedded. For these analyses, this disadvantage was of paramount importance.

The concept was proved to be valid by analysis of simple structures. Unfortunately, the complexity of the anchorage zone block analysis appears to have defied the ability of the ADINA concrete model to represent the material behavior accurately. Consequently, the embedded-bar concept could not accurately represent the true behavior of the rebars. Further testing with a more reliable concrete model or a test specimen of lesser complexity than the anchorage blocks investigated in this study is required before the embedded-bar concept can be correctly evaluated.

4.2.3 SAP2PR Finite-Element Program

Pressure loads or distributed loads constitute at least part of the load on most structures analyzed by 2-D finite-element computer programs. Unfortunately, not all finite-element codes allow the user to specify pressure loads directly. For codes such as these, only loads acting at the nodes are acceptable as input. Therefore, before the effects of a pressure load can be analyzed, the distributed loads must be converted into equivalent nodal loads. The computer program SAP2PR was written for

this purpose for the Digital Equipment Corporation PDP-10 in Fortran language; with minor modifications, the program can be executed on the IBM 360 or 370 computer. (A listing of the program and examples of input to and output from the program for an example problem have been prepared.¹⁷⁾

4.2.3.1 Program Input

The input cards for the program are of two types: model definition cards and load case definition cards. The model definition cards define the geometry of the finite-element analytical model, and the load case cards describe the pressure loadings. Details on the input cards have been recorded.¹⁷⁾

4.2.3.2 Program Operation

Equivalent nodal forces caused by distributed loads are given by the equation¹⁸⁾

$$\{F\} = -\int [N]^T \{p\} dV, \quad (4.11)$$

with the integration being over the volume of the loaded element. The matrix $[N]$ is composed of the shape functions as follows:

$$\left. \begin{aligned} n_{1,3i-2} &= h_i \\ n_{1,3i-1} &= 0 \\ n_{2,3i-2} &= 0 \\ n_{2,3i-1} &= h_i \end{aligned} \right\} \quad (i = 1, 2, \dots, m),$$

where h_i is the shape function for node i , $n_{j,k}$ is the term in the j th row and k th column of $[N]$, and m is the number of nodes that define the elements. The terms of the vector $\{p\}$ are the components of the distributed load in the global coordinate directions. Since $\{p\}$ is zero except on the face that is pressurized, it is only necessary to integrate over the length of the pressurized face. Numerical integration is used after the face has been mapped into the local 1-D u space as shown in Fig. 4.5. Assuming that one of the global coordinates (y or z) is a function of the other one, the following expression can be obtained:

$$\{F\} = \int_{-1}^1 [\bar{\mathbf{N}}]^T \{\bar{\mathbf{p}}\} du, \quad (4.12)$$

where

$$\{\bar{\mathbf{p}}\} = \begin{Bmatrix} \frac{dy}{du} \\ \frac{dz}{du} \end{Bmatrix} p_n$$

and p_n is the magnitude of the pressure, a function of u

$$p_n = \sum_{i=1}^m p_i h_i(u).$$

The matrix $[\bar{\mathbf{N}}]$ is composed of the shape functions for the nodes on the pressurized face.

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Fig. 4.5. Mapping of a pressurized face into a local one-dimensional space.

4.2.3.3 Program Output

Program output, which is compatible with ADINA,¹⁵ NONSAP,¹⁹ and STATIC-SAP,²⁰ is written onto a disk file. A complete listing of the program and a sample problem are contained in the appendixes of a published report.¹⁹

4.3 CONCRETE PROPERTIES FOR REACTOR APPLICATIONS — G. C. Robinson and D. J. Naus

The objectives of the concrete properties in a nuclear environment task are to provide basic information required to support development of improved analysis methods and to develop a bank of highly reliable data. Activities during this reporting period were directed at assembling information on available concrete property data and development of a performance specification for a concrete multiaxial material testing system.

4.3.1 Review of Concrete Property Data

Information on the effects of elevated temperatures and multiaxial loadings on concrete properties is being assembled. With increasing interest in the use of high-strength concretes (>41.4 MPa), data is also being assembled on requirements for producing these concretes and on concrete properties and research requirements. An interim report presenting information assembled to date has been published.²¹ A summary of this report follows:

Prestressed concrete pressure vessels (PCPVs) are designed to serve as primary pressure containment structures. The safety of these structures depends on a correct assessment of the loadings and proper design of the vessels to accept these loadings. Proper vessel design requires a knowledge of the component (material) properties. Because concrete is one of the primary constituents of PCPVs, knowledge of its behavior is required to produce optimum PCPV designs.

Concrete material systems are reviewed with respect to constituents, mix design, placing, curing, and strength evaluations, and typical concrete property data are presented. Effects of extreme loadings (elevated temperature, multiaxial, irradiation) on concrete behavior are described. Finally, specialty concrete material systems (high strength, fibrous, polymer, lightweight, refractory) are reviewed.

Based on the continuing overview of concrete material systems and on concrete property requirements for HTGR PCRVs, a test plan for determining needed concrete properties will be developed during the next reporting period. Contained in this plan will be a test matrix to determine the effects of elevated temperature and multiaxial and elevated-temperature-multiaxial loadings on the behavior of concrete. The study, which will

also be initiated during the next reporting period, will use a high-strength concrete (>55.2 MPa) such as that required for current-generation PCRV designs.

4.3.2 Concrete Multiaxial Material Testing System

Studies to develop concrete property data under multiaxial loadings that are representative of mechanical and thermal loadings likely to be encountered in a PCPV require the development of a relatively unique testing system; that is, the system must be capable of loading concrete specimens multiaxially in tension, in compression, or in a combination of tension and compression at either room or elevated temperatures. Available systems, such as the one located at the Bundesanstalt für Materialprüfung in West Berlin (FRG) and shown schematically in Fig. 4.6, all suffer from one of more of the following deficiencies: improper interface between specimen and load train; only compressive forces can be applied; descending portion of stress-strain curve cannot be obtained;

Fig. 4.6. Concrete multiaxial material testing system.

inability to apply cyclic loading profiles; loading in x -, y -, and z -directions is not independent; accuracy and reliability of specimen displacement measuring systems are questionable; and tests cannot be conducted at elevated temperatures.

Based on a review of concrete multiaxial property data and the test systems developed to obtain these data, a purchase specification²² for an elevated-temperature multiaxial concrete testing system has been developed and submitted to candidate vendors for response. The specification consists of six major sections: (1) general requirements, (2) load frame assembly, (3) environmental system, (4) specimen development and actuator stroke instrumentation, (5) interactive computer control and data acquisition system, and (6) functional requirements of computer programs for system control data acquisition and analysis. Additional details on each of these sections have been documented.²²

4.4 STEEL REINFORCEMENT AND PRESTRESSING FOR PRESTRESSED CONCRETE PRESSURE VESSELS — D. J. Naus

A review of steel reinforcement and prestressing for application to PCPVs was conducted. Information contained in the review is described below.²³

1. The design of prestressed concrete reactor vessels for current High-Temperature Gas-Cooled Reactor systems is based on the integral concept; that is, the entire pressurized gas circuit composed of reactor core, primary coolant system, and portions of the secondary coolant system are contained within a single vessel. The PCPV is, in essence, a spaced steel structure because its strength is derived from a multitude of steel elements made up of deformed reinforcing bars and prestressing, which are present in sufficient quantities to carry tension loads imposed on the vessel. The steel reinforcement and prestressing in the PCPVs thus play a vital role in assuring their structural integrity.

2. Bonded reinforcements (deformed bars) are provided in PCPVs to control the extent and width of cracks at operating temperatures, resist tensile stresses and computed compressive stresses for elastic design, and provide structural reinforcement where required by limit condition design

procedures. Reinforcing steel materials are reviewed as well as code requirements that must be met in the United States, United Kingdom, Germany, and France. Basic material property data are presented as well as the effect of temperature and irradiation on material properties.

3. Prestressing is applied to concrete members to eliminate or to reduce materially tensile stresses in concrete; that is, the prestress forces inhibit concrete cracking, thus enabling the entire concrete cross section to share in the stiffness of the vessel over a wide range of elastic behavior. The PCRV code requirements in the United States, United Kingdom, Germany, and France are reviewed with respect to the prestressing systems. Materials and fabrication procedures for production of prestressing wire, strand, rope, strip, and bar tendons are described. Material properties and the effect of temperature and irradiation on these properties are presented. Available linear and circumferential prestressing systems applicable to PCPVs are described. Incidences of tendon corrosion and corrosion protection techniques which are used for protecting prestressing steels in PCPVs are identified as well as the various code requirements. Also, code requirements for PCRV structural integrity testing and in-service surveillance are discussed.

4.5 INSTRUMENTATION EVALUATION AND DEVELOPMENT — D. J. Naus

An evaluation of the ability of acoustic emission (AE) to monitor the integrity of structural members such as PCPVs is continuing. During this reporting period a review was completed on applications of AE, as well as other nondestructive testing methods, to concrete materials systems and structures. Information contained in the review²⁴ is described as follows:

Nondestructive testing techniques that have been applied to concrete are overviewed and their usefulness and limitations presented. A general description of AE as a nondestructive testing technique is presented as well as examples of both nuclear and nonnuclear applications. A basis for the applicability of AE to concrete is established by relating the importance of concrete microcracking to the general behavior of concrete and by drawing an analogy between the concrete microcracking and inelastic deformations that are detectable in metallic materials by AE techniques. An

overview is presented of the limited applications of AE of concrete. Results obtained from AE monitoring of plain concrete specimens and post-tensioned concrete beams are presented. Potential applications of AE to PCPVs are identified as well as problem areas that have to be overcome if the technique is to be used to monitor the structural integrity of PCPVs.

Based on information in the above review, the following conclusions are derived.

- Of the concrete nondestructive testing methods overviewed, the ones that have the most potential for application to PCPVs include (1) pulse velocity to determine concrete uniformity; however, rebar congestion may present problems in certain areas; (2) neutron scattering or chemical methods to determine the cement content of a mix rapidly while still plastic; and (3) electrical methods to monitor internal moisture content.
- Acoustic emission principles are applicable to plain concrete and simple concrete structures.
- Acoustic emission can be used to locate flaws in simple concrete structures.
- Acoustic emission results obtained by various investigators indicate that potential PCRV applications include (1) weld monitoring (both during the welding process and later during in-service monitoring of weld integrity), (2) liner leak detection, (3) detection of corrosion and wire failure in prestressing tendons, (4) pipe integrity monitoring, (5) valve monitoring, (6) detection of incipient failure in bearings, (7) crack detection in relatively isolated areas, and (8) monitoring of mechanical components.
- Although AE was successfully used by Green²⁵ to monitor the structural integrity of a model PCRV, investigations are required before the technique can be used to monitor the structural integrity of an actual in-service PCPV because (1) frequency domains over which the vessel could be monitored would have to be established to eliminate background noise; (2) with the advent of wire winding to prestress the vessels circumferentially, access to the vessel concrete is limited (the longitudinal tendons could potentially be used as wave guides); and (3) currently, AE is primarily applicable to 1-D and 2-D

monitoring of structures; thus, for it to be applicable to PCPVs, which may have concrete thicknesses of several meters, 3-D techniques would have to be developed and refined. (Also, the extensive amount of both mild steel reinforcement and prestressing contained in these structures, as well as their sheer complexity, would complicate source location procedures.)

4.6 STRUCTURAL MODEL TESTS — C. B. Oland and G. C. Robinson

The objective of this task is to conduct PCRV-related model and structural tests in support of the development of reference and advanced HTGR plant designs. In line with this objective, two activities were initiated during this reporting period: (1) development of model testing techniques and (2) structural response testing of thermal barrier ceramic pads.

4.6.1 Development of Model Testing Techniques

Section III, Division 2 of the ASME "Code for Concrete Reactor Vessels and Containments"²⁶ requires model tests whenever a model of a prototype with characteristics similar to those of the current design has not been constructed and tested in accordance with code provisions if analytical procedures to predict ultimate strength and behavior in the range approaching failure are not established. Because the newer generation PCRV designs include an asymmetric cavity arrangement and offset core, existing model test data that were developed for single-cavity and symmetric multicavity PCRV designs might not meet the above requirement. In addition, the complexity of the new designs will require further development and verification of the 3-D finite-element analytical codes, especially for determination of long-term time-dependent behavior and for prediction of ultimate strength and behavior in the load range approaching failure conditions. Contingency plans are therefore being developed so that a PCRV model test can be conducted in a timely manner should it be established as a requirement for licensing.

Using results of a review of PCRV-related model tests,²⁷ we established that two areas require additional development: wire winding of

models and nonleak liner systems. A test program was therefore initiated to establish satisfactory methods for circumferentially prestressing PCRV models ranging in scale from about 1/30 to 1/10 and for lining the models so that they do not prematurely leak and terminate a test. Associated with this activity is an evaluation of the ability of fibrous concrete to produce cost- and performance-effective PCRVs. The techniques developed will be verified through two models like that shown in Fig. 4.7, which will be fabricated and tested to failure. Because one of the two models tested is to be fabricated from fibrous concrete, the results obtained will also provide an indication of the effectiveness of fibrous concrete for application to PCRV construction.

PHOTO 5277-77

Fig. 4.7. Prestressed concrete reactor vessel model.

Activities during this reporting period have focused primarily on design and fabrication of a nonleak liner system, development of model prestressing techniques, and development of ancillary equipment required for the model test technique validation studies.

4.6.1.1 Nonleak Liner System

The liner system for use in the model tests was designed and fabricated. The liner system consists of a flanged head to which a skirt section is joined. (Use of the flanged head eliminates the corner joint, which has been a weak point in previous liner designs.) A 12-gage AISI 1008 drawing-quality steel was selected as the liner material. After liner fabrication, studs were attached to the liner on the concrete side to prevent buckling of the liner and to tie deformations of the liner to those of the adjacent concrete. After stud attachment the liner assemblies were evacuated to verify that no leaks were present. Figure 4.8 presents a completed PCRV model liner system.

PHOTO 3087-81

Fig. 4.8. Completed PCRV model liner assembly.

4.6.1.2 Model Prestressing

Axial prestressing will use 19-mm-diam A 722 type I bars. A jack support assembly was fabricated to support the 200-kN axial prestressing force and permit access to the prestressing rod anchorage while the rod is maintained at the prestress force. A hydraulic-based tensioning system was developed to tension the prestressing rods. Strain gages were installed on selected prestressing rods, and the rods were calibrated by loading in tension and monitoring strain output as a function of load. These rods will be used to obtain axial load data as a function of pressure during the model test.

Requirements for circumferentially prestressing the models were developed. The models are required to be wound with 6.35-mm-diam prestressing wire. The wire shall have at least 1.79-GPa ultimate tensile strength and shall conform to ASTM A 648 Class 3 requirements. The force in the wire during application shall be 31.1 kN, and the wire shall be applied at a 6.35-mm pitch. Approximately 520 m of wire is required per model. A vendor has been identified who can meet these requirements for prestressing the models. Designs are being finalized for incorporation of the vendor's wire anchors into the mold and for fabrication of a fixture for adapting the models to the vendor's wire-winding machine.

4.6.1.3 Ancillary Model Testing Equipment

Development of ancillary equipment required for fabrication, shipping, and testing the PCR/V models was completed. Items included in this equipment include the mold for casting the models (Fig. 4.9); a shipping fixture for transporting the models to and from the wire-winding facility; an instrumentation support system, which will be used as a frame of reference for displacement measurements during the test; a base plate assembly, which will serve as the lower sealing surface for the PCR/V models and permit access to the inside of the pressurized cavity of the model for pressure ports and instrumentation leadthroughs; a slotted plate, which will serve as the bearing surface for the axial prestressing; a pressurization system capable of hydraulically loading the model to 62 MPa; and an 80-channel minicomputer controlled data acquisition system and associated software.

PHOTO 8055-82

Fig. 4.9. Mold for casting PCRV models.

4.6.2 Structural Response Tests of Thermal Barrier Ceramic Pads

In conformance with General Atomic Company's (GA's) specification for thermal barrier ceramic pads,²⁸ a test plan was prepared.²⁹ Included in the test plan are proposed methods for instrumenting, loading, and monitoring the ceramic pads. The purposes of the tests are to provide structural response and AE data for various configurations of ceramic support pads and accompanying interface materials. Work to date has included design and fabrication of the test fixture, procurement of the strain gages and low-profile load cell, and development of the data acquisition system. The first phase of the thermal barrier structural response tests will be completed during the next reporting period.

4.7 LINER, COOLING, AND INSULATION SYSTEMS FOR PRESTRESSED CONCRETE PRESSURE VESSELS — D. J. Naus

A state-of-the-art review on liner, cooling, and insulation systems for prestressed concrete pressure vessels has been made.³⁰ Information contained in that review is described as follows:

1. Liners, cooling, and insulation systems are an integral part in the performance of PCPVs. The liner serves as a number of separate functions: (1) it serves as part of the system of forms used in placing the concrete during construction, (2) it is the barrier preventing the escape of primary cooling gas and gaseous fission products, (3) it is an essential part of the system protecting the concrete from excessive temperature, and (4) parts of the liner in and near penetrations aid in transferring the loads on the penetration closures into the PCR structure through shear anchors embedded in the concrete. The insulation and cooling systems play a vital role in controlling the thermal gradient across the wall in the PCR and in preventing concrete temperatures from reaching values where mechanical properties deteriorate.

2. Requirements are presented for liner, cooling, and insulation systems in PCRVs. Current codes (ACI/ASME, British Standards Institution, German, French, and International Standards Organization) are reviewed with respect to design, analysis, materials, and fabrication and installation. Hot liners and replaceable thermal barriers are discussed. Experimental results obtained in support of liner, cooling, and insulation systems are summarized. An overview of systems that have been used for PCRVs and proposed for PCPVs is presented.

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5. HTGR STRUCTURAL MATERIALS

P. L. Rittenhouse

5.1 INTRODUCTION — P. L. Rittenhouse

The focus of work on structural materials at ORNL during the year changed away from advanced gas-cooled reactors with core outlet temperatures of up to 950°C toward the Steam Cycle-Cogeneration (SC-C) plant with a maximum average outlet temperature of 700°C. Programs now emphasize alloys and ceramics that have been selected as reference materials for critical components of the SC-C system, such as thermal barriers, heat exchangers, Prestressed Concrete Reactor Vessel (PCRV) liners and penetrations, reactor internals, and the core support structure. Although we expect these reference materials to perform satisfactorily, additional information and data are needed for detailed design and to ensure that the materials are suitable from all viewpoints, including licensability and long-term service.

The other principal participant in U.S. efforts on High-Temperature Gas-Cooled Reactor (HTGR) structural materials for SC-C systems is the General Atomic Company (GA); a small effort on SC-C alloys is conducted by the General Electric Company (GE), but its main task remains alloys for advanced systems. A Materials Working Group, composed of contractor and government members, functions to provide consistency in the scope of the programs of the three laboratories.

During 1981 the HTGR Structural Materials Program at ORNL was divided into six major areas:

- mechanical properties,
- corrosion of alloys by HTGR helium,
- joining technology,
- structural ceramics,

- fission product-materials interactions, and
- liner and penetration studies.

These areas are discussed in separate sections. Although emphasis is now on SC-C materials, much of the information reported involves completion of studies relevant to materials for advanced systems.

5.2 MECHANICAL PROPERTIES — C. R. Brinkman

5.2.1 Tensile Properties and Creep of Alloys and Weldments — H. E. McCoy and J. F. King

Our creep work is concentrated on the materials described in Tables 5.1 and 5.2. These materials are being studied as base metal and weldments in the as-received and aged conditions.

Tensile tests were run on all alloys, and some of the results are summarized in Fig. 5.1 for four of the stronger alloys. The tensile and yield strengths of HD556 are appreciably above those of the other three alloys over the entire temperature range studied. Incoloy 802 has a lower

Fig. 5.1. Tensile properties of several alloys (strain rate = 0.05/min).

Table 5.1. Alloy characterizations

Material	Heat	Source	Product form	Grain size (μm)	Heat treatment
Alloy 617	XX01A3US	INCO ^a	12.7-mm (1/2-in.) plate		Solution annealed by vendor
Hastelloy X	2600-3-4936	Cabot ^b	12.7-mm (1/2-in.) plate	80	Solution annealed by vendor
	2600-3-2792	Cabot	31.8-mm (1 1/4-in.) bar	55	Solution annealed by vendor
2 1/4 Cr-1 Mo	X-6216	B&W ^c	51-mm-OD (2.0-in.) tube	27	Isothermally annealed ^d
	36202	B&W	51-mm-OD (2.0-in.) tube	20-27	Isothermally annealed ^d
	72768	B&W	51-mm-OD (2.0-in.) tube	27	Isothermally annealed ^d
9 Cr-1 Mo	316381				Annealed
HD556	8556-5-7305	Cabot	6.4-mm (1/4-in.) plate		Unknown, by vendor
Inconel 618	Y09B9	INCO	12.7-mm (1/2-in.) plate		Unknown, by vendor
Incoloy 802	HH6863B	INCO	7.9-mm (0.312-in.) plate		Solution annealed by vendor

^aINCO, International Nickel Co.

^bCabot, Cabot Corp.

^cB&W, Babcock and Wilcox Co.

^d927 $\pm$ 14°C for 30 min, cooled to 704 $\pm$ 14°C at a maximum rate of 83°C/h, held at 704 $\pm$ 14°C for 2 h, and cooled to room temperature at a maximum rate of 6°C/min.

Table 5.2. Alloy compositions

Material	Content (%)													
	Ni	Cr	Co	Mo	Fe	Al	Si	C	Mn	S	W	Ti	Cu	Others
Alloy 617	57.35	20.30	11.72	8.58	1.01	0.76	0.16	0.07	0.05	0.004				
Hastelloy X 4936	Bal.	21.82	1.68	9.42	19.09		0.44	0.07	0.58	<0.005	0.63			
2792	Bal.	21.25	1.94	8.99	18.96		0.41	0.10	0.57	<0.005	0.56			
2 1/4 Cr-1 Mo X-6216		2.19		1.00	Bal.		0.26	0.12	0.44	0.016				
36202		2.24		0.96	Bal.		0.34	0.12	0.43	0.023				
72768		2.16		0.94	Bal.		0.38	0.10	0.41	0.012				
9 Cr-1 Mo	0.4	8.83		1.0	Bal.		0.6	0.0803	0.5	0.0208				0.02 V
HD556	19.44	21.39	19.08	2.72	Bal.	0.26	0.49	0.11	1.20	<0.005	2.08			0.01 La, 0.76 Cb + Ta
Inconel 618	56.49	22.5			14.79	0.01	0.021	0.05	0.09	0.005	5.85		0.04	
Incoloy 802	33.45	19.74			44.57		0.37	0.30	1.11	0.001			0.46	

tensile strength at 25°C than the other alloys, but at all other conditions the yield and tensile strengths of Inconel 617, Hastelloy X, and Incoloy 802 are equivalent.

Suitable weldments were made in all alloys studied. Hastelloy X was joined by the gas tungsten arc (GTA) and shielded metal arc weld (SMAW) welding processes, and both Hastelloy X and Hastelloy S filler rod were used. In general, transverse weld samples failed in the base metal in tensile tests below 500°C and in the weld metal at higher temperatures. Inconel 617 exhibited the same trend. The HD556 weld metal was slightly weaker than the base metal, and failure occurred in the weld metal. Incoloy 802 was joined with Inconel 617 filler metal, and failure consistently occurred in the weaker Incoloy 802 base metal.

The creep tests in progress on these materials are summarized in Table 5.3. The comparative creep strengths are plotted in Fig. 5.2 for 760°C. Three of the alloys have very similar creep strengths at this temperature, but Hastelloy X is weaker by a factor of about 2 in stress. All the alloys except Incoloy 802 carburized in HTGR-helium, but this reaction had no detectable effect on the creep rate.

Fig. 5.2. Comparative creep rates of four alloys at 760°C.

Table 5.3. Status of creep tests in progress

Test number	Material	Environment	Temperature (°C)	Stress		Time (h)	Approximate creep strain (%)
				(MPa)	(ksi)		
18434	Hastelloy X, heat 4936, flat	He	649	138	20	41,227	11.6
18150	Hastelloy X, heat 4936	He	649	138	20	42,475	8.4
18448	Hastelloy X, heat 2792	Air	760	69	10	39,926	10.8
22891	Hastelloy X, heat 4284	He	649	207	30	959	6.6
21565	Hastelloy X, heat 4936, aged 20,000 h at 538°C in inert gas	He	538	345	50	15,619	12.3
22193	Hastelloy X, heat 4936, aged 20,000 h at 871°C in HTGR-He	He	871	35	5	8,949	16.8
20559	Hastelloy X weld, X/X/X, GTA, ^a aged 2000 h at 593°C in inert gas	He	593	241	35	17,221	4.3
18430	Hastelloy X weld, X/S/X, GTA, ^a	He	593	241	35	41,463	5.4
21561	Hastelloy X weld, X/S/X, GTA, ^a aged 2000 h at 593°C in inert gas	He	593	241	35	15,742	3.0
22210	Hastelloy X weld, X/S/X, GTA, ^a aged 10,000 h at 593°C in inert gas	He	593	275	40	6,383	2.9
22186	Hastelloy X weld, X/X/X, GTA, ^a aged 20,000 h at 871°C in HTGR-He	He	871	35	5	8,146	3.4
22679	Hastelloy X-alloy 800H weld, Incoweld A filler, aged 10,000 h at 538°C in inert gas	He	538	345	50	3,454	1.5
22239	Hastelloy X-alloy 800H weld, Incoweld A filler, aged 10,000 h at 482°C in inert gas	He	482	414	60	3,166	8.9
22737	Hastelloy X-alloy 800H weld, Inconel 82 filler, aged 10,000 h at 538°C in inert gas	He	538	345	50	2,518	1.9
21602	Inconel 617, Lot XX01A3US	He	871	21	3	10,771	0.2

Table 5.3. (Continued)

Test number	Material	Environment	Temperature (°C)	Stress		Time (h)	Approximate creep strain (%)
				(MPa)	(ksi)		
21603	Inconel 617, Lot XX01A3US	Air	871	21	3	11,410	0.3
19761	Inconel 617, Lot XX01A3US	Air	871	34	5	31,477	27.2
19182	Inconel 617, lot XX01A3US	He	649	207	30	36,734	27.2
22753	Inconel 617, lot XX9A4UK	He	704	207	30	862	0.6
22755	Inconel 617, lot XX9A4UK	He	760	138	20	503	0.7
22754	Inconel 617, lot XX9A4UK	He	760	173	25	742	2.0
22751	Inconel 617, lot XX9A4UK	He	649	276	40	1,172	3.0
20534	Inconel 617, aged 20,000 h at 538°C in inert gas	He	538	414	60	20,003	15.1
20553	Inconel 617, all weld metal	He	649	241	34	17,226	1.3
22236	Modified Inconel 617, alloy J492	He	760	69	10	3,765	1.1
22237	Modified Inconel 617, alloy J492	He	871	34	5	3,739	2.6
22947	Modified Inconel 617, alloy J492	Air	760	207	30	12	3.9
22199	Inconel 618, 1 h at 1177°C	He	760	69	10	8,793	9.0
22205	Incoweld A	Air	649	103	15	8,102	0.8
22181	Incoloy 802	Air	760	69	10	9,837	1.0
22409	Incoloy 802	He	760	69	10	8,638	1.0
22233	Incoloy 802	He	871	21	3	4,005	0.6
22183	Incoloy 802, weld	Air	760	69	10	9,696	0.2
20548	HD556	He	871	34	5	17,343	0.6
20536	HD556, weld	Air	871	34	5	19,527	3.4

Table 5.3. (Continued)

Test number	Material	Environment	Temperature (°C)	Stress		Time (h)	Approximate creep strain (%)
				(MPa)	(ksi)		
22823	2.25 Cr-1 Mo steel, heat 72768	Air	482	207	30	2,724	5.7
18860	2.25 Cr-1 Mo steel, heat 72768	He	538	69	10	37,685	12.9
18446	2.25 Cr-1 Mo steel, heat 72768	He	649	21	3	40,216	1.5
18252	2.25 Cr-1 Mo steel, heat 72768	He	538	41	6	42,297	0.3
18508	2.25 Cr-1 Mo steel, heat 72768, flat specimen	He	538	41	6	40,914	1.6
19945	2.25 Cr-1 Mo steel, heat 72768	He	538	41	6	30,766	0.8
22758	2.25 Cr-1 Mo steel, heat 72768	Air	593	103	15	21	2.4
22752	2.25 Cr-1 Mo steel, heat 72768	Air	649	48	7	1,028	7.3
22757	2.25 Cr-1 Mo steel, heat 36202	Air	649	48	7	21	0.1
20508	9 Cr-1 Mo steel	Air	538	103	15	24,866	20.8
22214	Alloy 10801, solution annealed	He	760	138	20	5,614	3.5
22222	Alloy 10802, solution annealed	He	871	69	10	4,888	7.3
22746	Inconel 713LC, EB ^b weld	Air	649	354	50	1,557	1.2
22745	MA956, EB-6 weld	Air	649	28	4	1,582	0.3
20560	SiO ₂ , Corning 7941, compressive	He	871	14	2	17,104	5.5
20555	SiO ₂ , Corning 7941, compressive	He	816	14	2	17,177	2.5
21597	SiO ₂ , Corning 7941, compressive	He	816	41	6	11,834	1.1
22758	SiO ₂ , TM ^c (four-point bending)	He	649	21	3	1,173	0.1
22748	SiO ₂ , TM ^c (four-point bending)	He	538	21	3	1,478	1.5

^aGTA, gas tungsten arc.^bEB, electron beam.^cTM, Thermo Materials.

5.2.2 Aging Studies — H. E. McCoy and J. F. King

Many of the alloys being studied experience two changes from exposure to HTGR-helium for long periods of time at elevated temperatures. They can undergo microstructural changes from exposure to elevated temperatures, and they can be carburized by the HTGR-helium. Both of these processes generally result in precipitates forming in the materials, with a consequential reduction in ductility. We are studying these aging effects for several materials, and the programs are summarized in Table 5.4. In those cases for which the status is marked as complete, the aging exposure has been completed, and various mechanical property tests are in progress.

The studies are most advanced on Hastelloy X, and results for this material will be used to illustrate some of the effects noted. The influence on the room-temperature impact properties as a result of aging in inert gas is shown in Fig. 5.3. Although the impact energy drops more quickly as a result of aging at 871°C than at 704°C, the residual value after aging 10,000 h at either temperature is 5 to 10 J. The scanning electron micrographs in Fig. 5.4 show that the drop in impact energy is accomplished by a shift in the fracture mode from transgranular to intergranular. This change is probably due to extensive intergranular precipitation in the material. Inconel 617 and HD556 behave similarly, but Inconel 618 shows little embrittlement.

Tensile tests were run at room temperature on both base metal and transverse weld samples following aging in either inert gas or HTGR-helium. The test results for the yield and ultimate tensile stresses are given in Fig. 5.5. The strength changes due to aging at 538 and 871°C are very small, but the changes due to aging at 704°C are quite significant. The samples aged in an inert gas reach their peak strength after aging for 2500 h and decrease in strength with longer aging. The samples aged in HTGR-helium exhibit larger strength changes than those aged in inert gas, and the changes seem to increase with increasing aging time. Fracture strain measurements for these same specimens are shown in Table 5.5. In the two instances in which direct comparisons can be made, the fracture strain was less for samples aged in HTGR-helium than for samples aged in inert gas.

Table 5.4. Summary of ORNL aging programs

Material	Aging conditions			Status
	Temperature (°C)	Time (h)	Environment	
Base metals				
Hastelloy X	538	2.5, 10, 20	Inert	Complete
Hastelloy X	704	2.5, 10, 20	Inert	Complete
Hastelloy X	871	2.5, 10, 20	Inert	Complete
Incoloy 802	593, 704, 871	10	HTGR-He	Complete
Inconel 617	538	2.5, 10, 20	Inert	Complete
Inconel 617	704	2.5, 10, 20	Inert	Complete
Inconel 617	871	2.5, 10, 20	Inert	Complete
Hastelloy X	593, 704, 871	10, 20	HTGR-He	Complete
Hastelloy X	593, 704, 871	50	HTGR-He	32,000 h
Inconel 617	593, 704, 871	10, 20	HTGR-He	Complete
Inconel 617	593, 704, 871	50	HTGR-He	32,000 h
Inconel 618	593, 704, 871	10	HTGR-He	Complete
HD556	593, 704, 871	10	HTGR-He	Complete
Modified Inconel 617	593, 704, 871	10	HTGR-He	Complete
Welds ^a				
X/X/X, GTA	538, 704, 871	2	Inert	Complete
X/82/Incoloy 800H, GTA	427, 538, 649	2	Inert	Complete
X/X/X, GTA	593, 649, 760, 871	2, 10	Inert	Complete
X/X/X, SMAW	593, 649, 760, 871	2, 10	Inert	Complete
X/S/X, GTA	593, 649, 760, 871	2, 10	Inert	Complete
X/182/800H, GTA	482, 538, 649	2, 10	Inert	Complete
X/A/800H, SMAW	482, 538, 649	2, 10	Inert	Complete
X/X/X, GTA	593, 704, 871	10, 20	HTGR-He	Complete
X/X/X, GTA	593, 704, 871	50	HTGR-He	32,000 h
617/617/617, GTA	593, 704, 871	10, 20	HTGR-He	Complete
617/617/617, GTA	593, 704, 871	50	HTGR-He	32,000 h
802/617/802, GTA	593, 704, 871	10	HTGR-He	Complete
618/618/618, GTA	593, 704, 871	10	HTGR-He	Complete
556/556/556, GTA	593, 704, 871	10	HTGR-He	Complete
M617/M617/M617, GTA	593, 704, 871	10	HTGR-He	Complete

^aThe following designation is used for welds: Base metal 1/filler metal/base metal 2; GTA, gas tungsten arc process; and SMAW, shielded metal arc weld.

Fig. 5.3. Charpy-V impact energy of Hastelloy X at 25°C following aging in inert gas at the indicated condition.

ORNL-DWG 81-18289A

Fig. 5.4 Scanning electron micrographs of Hastelloy X fracture surfaces following impact testing at 25°C.

ORNL-DWG 81-9212RA

Fig. 5.5. Tensile properties of Hastelloy X at 25°C following aging at the indicated condition.

Table 5.5. Tensile fracture strains of Hastelloy X samples tested at room temperature

Aging conditions		Fracture elongation (%)	
Time (h)	Temperature (°C)	Aged in inert gas ^a	Aged in impure He ^b
0	0	52	
2,500	538	55	
10,000	538	43	43 ^c
20,000	538	47	24 ^c
2,500	704	9	
10,000	704	18	11
20,000	704	10	8
2,500	871	15	
10,000	871	15	10
20,000	871	18	11

^aTest section 6.35 mm (1.4 in.) in diameter by 31.8 mm (1.25 in.) long.

^bTest section 3.18 mm (1.8 in.) in diameter by 25.4 mm (1.00 in.) long.

^cAged at 593°C.

Several transverse weld samples of Hastelloy X were also aged in inert gas and HTGR-helium under similar conditions, in which comparisons can be made of fracture strain after testing in the two environments. As shown in Table 5.6, two of three of the aging conditions clearly resulted in lower fracture strains from testing in HTGR-helium than from testing in inert gas.

Table 5.6. Room-temperature tensile fracture strains for transverse Hastelloy X welds aged for 10,000 h

Aging temperature (°C)	Elongation at rupture (%)	
	Inert environment	Impure He
593	18.5	30.7
650	6.1	
704		4.1
760	6.5	
871	10.6	4.6

5.2.3 Fatigue — J. P. Strizak

An investigation of the effects of thermal aging and environment on the low-cycle fatigue behavior of Hastelloy X and Inconel 617 was completed during the current reporting period.

Figures 5.6 and 5.7 compare data generated for Hastelloy X in the solution-annealed or solution-annealed-and-aged conditions, in which the aging and test temperature were identical. Data plotted in Fig. 5.6 were generated at 538°C in either air or impure helium. Only a small (beneficial) effect of the helium environment is shown on resultant fatigue life compared with data obtained in air, but thermal aging before testing can reduce fatigue life, depending on the prior exposure time. Figure 5.7 shows that a thermal aging treatment for 10,000 h decreases the resultant

Fig. 5.6. Comparison of strain-controlled fatigue data generated at 538°C in air and impure helium for Hastelloy X in several conditions.

Fig. 5.7. Comparison of strain-controlled fatigue data generated at 871°C in air and impure helium for Hastelloy X in several conditions.

low-cycle life when the aging and test temperatures are 871°C but that cycle life is restored when the thermal aging time is extended to 20,000 h. These changes are probably due to subtle changes in the microstructure and resultant changes in ductility.

Results of similar tests on the effects of thermal aging conducted on Inconel 617 are plotted in Figs. 5.8 through 5.10. The results indicate a mixed response. At 538°C there is a clear trend that thermal aging results in improved cycle life with increasing aging time. Aging for 10,000 h before testing at 704°C reduces fatigue life somewhat at this temperature (Fig. 5.9), but aging for 20,000 h at this temperature improved fatigue life slightly. Similar trends were noted in the data generated at 871°C (Fig. 5.10). These changes were attributed to microstructural changes in posttest examination of the specimens. Results of testing of Inconel 617 in impure helium (Fig. 5.10) indicated an improved cycle life in the helium environment.

Fig. 5.8. Comparison of low-cycle fatigue behavior of Inconel 617 tested in air and impure helium at 538°C.

Fig. 5.9. Comparison of low-cycle fatigue behavior of Inconel 617 tested in air and impure helium at 704°C.

Fig. 5.10. Comparison of low-cycle fatigue behavior of Inconel 617 tested in air and impure helium at 871°C.

Investigation of the effects of thermal aging and environment on the low-cycle fatigue behavior of Hastelloy X and Inconel 617 has been reported in detail.¹ The report includes an analysis of continuous-cycling fatigue data obtained from room temperature to 427°C for Hastelloy G, Hastelloy X, Hastelloy C-276, and Hastelloy C-4, an effort undertaken in support of ASME code development (i.e., development of fatigue curves for Sect. III of the *ASME Boiler and Pressure Vessel Code*).

Current, ongoing work focuses on the creep-fatigue and high-cycle fatigue behavior of Hastelloy X at 760°C for HTGR-SC-C applications. The test matrix for low-cycle and creep-fatigue tests on solution-annealed and thermally aged conditions is given in Table 5.7. Tests completed to date are summarized in Table 5.8. Similar tests in an HTGR-helium environment will begin about April 1982.

A fatigue test system was recently outfitted for high-cycle fatigue testing of Hastelloy X. Tests will be conducted in air at 760°C for lifetimes of up to 10^8 cycles, starting about March 1982.

5.2.4 Stress Relaxation of Ceramics — K. C. Liu

Creep and stress relaxation in structural ceramics are important to the design and analysis of the HTGR core support structures. Analyses of the response of core support pads to potential thermal transients depend on many variables, including knowledge of creep and stress relaxation behavior of the structural ceramics. Both properties are similar; however, no explicit relationship has been established for a given structural ceramic material. The objectives of this experimental program are (1) to develop a stress relaxation testing system capable of testing brittle material at high temperatures and (2) to conduct a series of stress relaxation tests to determine the basic behavior of the candidate materials in order to formulate constitutive relations for use in high-temperature design and analysis. A stress relaxation testing system designed to test structural ceramics by four-point bending in a controlled HTGR-helium atmosphere has been developed at ORNL. The system is complete, with an

Table 5.7. Test matrix for strain-controlled
low-cycle and creep-fatigue tests in air
at 760°C on Hastelloy X

Material condition ^a	Total strain range ^b (%)	Hold period ^c (h)	Expected lifetime (cycles)
Solution annealed	2	None	300
Solution annealed	2	1.0 C	200
Solution annealed	2	1.0 T	250
Solution annealed	2	1.0 T + 1.0 C	200
Solution annealed	0.5	None	8400
Solution annealed	0.5	0.5 C	2000
Solution annealed	0.5	0.5 T	6000
Solution annealed	0.5	0.5 + 0.5 C	2000
Aged at 704°C	2	None	300
Aged at 704°C	2	1.0 C	200
Aged at 704°C	2	1.0 T	250
Aged at 704°C	0.5	0.5 C	2000
Aged at 871°C	2	1.0 C	200
Aged at 871°C	2	1.0 T	250

^aAging, where indicated, performed in argon for 20,000 h.

^bRamp rate of 4×10^{-3} /s.

^cC, hold period at peak compressive strain; T, hold period at peak tensile strain.

Table 5.8. Results of low-cycle and creep-fatigue tests conducted on Hastelloy X at 760°C in air

Specimen ^a	Heat treatment	Total strain range (%)	Hold period ^b (h)	Cycles to failure	Time to failure (h)
HXL-96	Solution annealed	2.0	None	331	0.9
HXL-97	Solution annealed	2.0	1.0 C	147	147.1
HX05	Solution annealed	2.0	1.0 T	214	214.6
HX06	Solution annealed	2.0	1.0 T + C	195	390.5
HX07	Solution annealed	0.5	None	16,077	11.2
HX09	Solution annealed	0.5	0.5 C	1,622	812.1
HX10	Solution annealed	0.5	0.5 T	>1,007	^c

^aHeat 2600-3-4936.

^bC, hold period at peak compressive strain; T, hold period at peak tensile strain.

^cTest in progress.

environmental chamber and a resistive heating furnace capable of heating a ceramic specimen to above 1100°C. It is a closed-loop controlled electromechanical tester with a numerical controller. Three modes of test control in load, strain, and stroke are possible, provided that the respective transducers are included in the testing system.

Figure 5.11 is a close-up view of the main load frame and the four-point bending mechanism, designed to test a bar specimen with a maximum width of 7.5 mm. Four load points are equally spaced at 7.5 mm. The maximum thickness of the specimen is governed by the gage length (the distance between the inner load points) so that bending in the region is a constant maximum moment. A thickness of 2.5 to 3.0 mm is appropriate in this case.

The supporting structure of the bending mechanism is a four-post rig with a circular platform. The structure has a shallow sunken floor to seat the bending mechanism in the center. To bend the specimen, the load

ORNL-Photo 1245-82

Fig. 5.11. Close-up view of stress relaxation apparatus for testing structural ceramics by four-point bending.

is applied to the upper plunger via the pull-rod assembly, consisting of a straight pull rod and a rectangular load frame with a compression anvil attached to the upper crossbar. The compression anvil is seated in a shallow spherical depression machined in the center of the flat top surface of the upper plunger to achieve an equal distribution of loads on the load points. The upper plunger is designed to travel only in the vertical direction, thus providing a reasonable way to maintain the spacing of the load points during testing.

Load is measured by a load cell, which also acts as a joint between the pull rod and the load applicator. The deflection between the outer and inner load points is measured by a linear variable differential transformer (LVDT) displacement transducer located immediately below the base plate (not visible in Fig. 5.11). The outer fiber strain is calculated from the beam equation based on the deflection. For tests at elevated temperatures in controlled gas, the entire testing apparatus is enclosed in the environmental chamber and heated by the furnace.

As a preliminary check of the system, a four-point bending test was performed at room temperature on a type 316 stainless steel specimen. The modulus of elasticity was then calculated on the basis of the beam equation, which yielded a value of 152.4 GPa. This is about 20% below the average value of 191.4 GPa reported for room-temperature tensile tests of the material. The discrepancy could be attributed to statistical scatter and the difference in testing methods, such as bending compared to tension, except that the variance is somewhat excessive. Other sources of error in the four-point bending test include

1. friction forces developed under load points;
2. changes in spacing between load points, resulting in unequal distribution of loads;
3. warped specimen or uneven contacts in loading mechanism; and
4. nonideal material behavior.

Efforts are now being directed to analyzing the extent of experimental error resulting from the imperfections inherent in the system and to developing analytical methods to correct the inherent errors, if such

errors would be reproducible. An in situ calibration of the load cell is under way. A specimen made of a high-yielding-strength steel has been machined and is being instrumented with strain gages to measure the bending strains.

5.2.5 Creep of Ceramics — H. E. McCoy

Several compressive creep tests were run on Corning 7941 fused SiO_2 from 816 to 982°C; two tests have reached 17,000 h. Sample-to-sample variation was a problem in determining the creep response of the material. A more serious problem was Corning's inability to produce more of this material, and the test program has been shifted to a fused silica produced by Thermo Materials. The test temperature was also adjusted to the 500 to 700°C range.

Two four-point bending rigs have been in service during the past year to evaluate the creep response of Si_3N_4 . Most of the effort went into a series of small modifications to make the test equipment operational for long periods at 1000°C. Current reactor concepts do not utilize Si_3N_4 , and the equipment is now being used to study fused silica. Two static fatigue tests have been in progress for about 1000 h at 538 and 649°C.

5.3 CORROSION OF ALLOYS BY HTGR-HELIUM — H. Inouye and J. H. DeVan

This task investigates the corrosion behavior of selected alloys in simulated HTGR-helium and is aimed at understanding the processes that occur, at elucidating the effects of the processes on mechanical properties, and at identifying the controlling variables. During this reporting period tests were run specifically to establish the dependence of the carburization of Hastelloy X and alloy 800H on the H_2O supply rate in the test gas. In contrast to the prior runs in which the test specimens were exposed to the test gas along a temperature gradient, these tests were run isothermally so that the effects of the helium flow rate and the surface area of the test specimens exposed to the test gas could be determined. Because impurity depletion was also expected, the experimental method was modified so that the depletion-generation of the helium impurities could be determined by analyzing the H_2 , CH_4 , and CO at the

retort inlet and outlet. In the H₂O measurements, the Panametrics hygrometer probe installed near the retort inlet was supplemented by a second probe immediately downstream of the outlet. Both probes were directly in the gas stream and were left in place during a run so that the typical inertia of the probes to concentration changes at the very low H₂O levels was not a factor. Each 6.35-mm-diam by 25-mm-long specimen contained two shallow diamond saw cuts spaced about 22 mm apart to measure their dilation. The carbon analysis reported below represents a single Leco combustion gas chromatographic analysis of a 1-mm-thick section from the center of the specimen. However, duplicate analyses were also made when the results were suspect. In the worst case, duplicate analyses varied by 10 to 20% but were usually within a few percent.

5.3.1 Carburization and Dilation — H. Inouye

Previously we reported that dilation resulting from the carburization of Hastelloy X and alloy 800H followed the general relationship $\Delta L \approx \Delta C^n$, where ΔL is the dilation, ΔC is the carbon change, and n is a factor of less than 1 from 750 to 1000°C for exposures as long as 4000 h (ref. 2). Revised plots of dilation versus carbon change based on additional tests at 1000°C are shown in Figs. 5.12 and 5.13. For both alloys, ΔL varies almost linearly with ΔC up to levels of about $10^4 \mu\text{g}/\text{cm}^2$ and becomes independent of ΔC above this value.

The dilation per se represents only a small amount of dimensional instability (0.6% or less); however, as postulated earlier,³ the dilation represents a creep strain caused by a carburization-induced stress. When this stress is superimposed on a creep stress, it may cause accelerated creep rates⁴ and early rupture,⁵ as indicated in Table 5.9 (refs. 4-6). This apparent degradation of the creep strength of Hastelloy X was estimated by determining the creep stress corresponding to the Larson-Miller parameter $P = (^\circ\text{F} + 460)(13.14 + \log t_{1\%})(10^{-3})$ and $P = (^\circ\text{F} + 460)(17.16 + \log t_r)(10^{-3})$, where $t_{1\%}$ is the time (h) to 1% strain and t_r is the rupture life (h) (ref. 6) in the HTGR-helium. The difference between this stress and the applied creep stress represents the environmental effect expressed as an induced internal stress, or (alternatively, as proposed by

Fig. 5.12. Stress generation during carburization in 50.7 H₂-5.1 CH₄-5.1 CO-0.15 H₂O Pa (500 H₂-50 CH₄-50 CO-0-1.5 H₂O μ atm) causes Hastelloy X to dilate.

Fig. 5.13. Stress generation during carburization in 50.7 H₂-5.1 CH₄-5.1 CO-0.15 H₂O Pa (500 H₂-50 CH₄-50 CO-0-1.5 H₂O μ atm) causes alloy 800H to dilate.

Table 5.9. Effect of specimen geometry on the creep behavior of Hastelloy X in HTGR-helium

Temperature (°C)	Stress (MPa)	Specimen diameter (mm)	Time (h) to	
			1% Strain	Rupture
871	34.5	Control	1325 ^a	
871 ^b	34.5	9.5	800 ^c	
871 ^b	34.5	4.8	525 ^c	
900	49.0	Control		344 ^a
900 ^d	49.0	8.0		225 ^e
900 ^d	49.0	4.0		137 ^e
900 ^d	49.0	1.75 ^f		80 ^e
900 ^d	39.2	4.0		225 ^e

^aSource: J. Hadley, *Hastelloy X Creep Properties*, BIPS-GDS-A0498, AiResearch, Phoenix, Ariz., August 1977.

^bTests in 152 H₂-45.6 CO-5.1 H₂O-0.2-0.5 CO₂ Pa (1500 H₂-450 CO-50 CH₄-50 H₂O-2-5 CO₂ μ atm).

^cSource: General Atomic Company, *Mechanical Properties Test Data for Structural Materials Quart. Prog. Rep.* Oct. 31, 1977, ORNL-5349, December 1977, p. 288.

^dTests in 20.3 H₂-10.1 CO-0.5 CH₄-0.3 H₂O-0.2-0.3 CO₂ Pa (200 H₂-100 CO-5 CH₄-3 H₂O-2-3 CO₂ μ atm).

^eSource: K. Kiuchi and T. Kondo, *Metallurgical Factors Influencing the Creep Behavior of Hastelloy X*, JAERI-8153, Japanese Atomic Energy Research Institute, Tokai-mura, Ibaraki-ken, Japan, March 1979.

^fPlate thickness.

other investigators) as a reduction in the creep strength due to the removal of alloying elements from solution as carbides.⁵ These calculations, presented in Table 5.10, show that the environmental stress can be a significant fraction of the applied creep stress and, as expected, increases with the surface-to-volume ratio (A/V) of the test specimens. Pairs of data points at A/V values of 0.50 and 1.00 suggest that the environmental stress also increases as the test temperature and

Table 5.10. Dependence of environmentally induced stresses in Hastelloy X on the specimen surface-to-volume ratio (A/V)

Temperature (°C)	Creep stress (MPa)	A/V	Calculated environmental stress (MPa)
871	34.5	0.50	5.5
871	34.5	0.42	8.3
900	49.0	0.50	4.8
900	49.0	1.00	9.7
900	49.0	1.34	16.5
900	39.2	1.00	13.1

creep stress are lowered. On this basis, dilation of alloys below 1000°C is expected to exhibit maxima at some intermediate temperature and to exceed the limit of 0.6% measured at 1000°C.

5.3.2 Depletion-Generation of HTGR-Helium Impurities — H. Inouye

Depletion of H₂O and CH₄ by gas phase and gas-metal reactions generates H₂ and CO in retorts. Water vapor depletion results in a test gas that is more carburizing at the downstream positions than at the upstream positions.^{2,7} When both H₂O and CH₄ are depleted, the test gas is most carburizing at an intermediate position in the retort, as shown by the plot in Fig. 5.14. Comparison of the separate plots for Hastelloy X, alloy 800H, and iron show the effect of their composition on carburization only at positions upstream and near the maxima. Downstream of the maxima, no significant difference in the carbon uptake is noted between the three alloys, presumably because the supply rate of CH₄ governs their carburization rates. Figure 5.15 shows the effect of increasing the impurity supply rate by an order of magnitude on the carburization of the Hastelloy X specimens. One effect of the higher supply rate is a shift in the position of the carburization maximum further downstream, which increases the maximum carburization level by only about 25%. In addition, the upstream positions are less carburizing while the downstream positions

Fig. 5.14. Carburization is position dependent because of H₂O and CH₄ depletion.

Fig. 5.15. Carburization of Hastelloy X depends on HTGR-helium flow rate.

are more carburizing at the higher supply rates. The origin of the carburization maxima is due to the depletion of H_2O in the test gas, as shown in Fig. 5.16. In the very dry test gas [1 mPa H_2O (0.01 μatm)], carburization shows neither a strong position dependence nor a maximum. Table 5.11 lists the generation-depletion of the HTGR-helium impurities corresponding to the carburization curves shown in Figs. 5.14 through 5.16 at equivalent retort loadings (i.e., same specimen surface areas exposed to the test gas). These measurements show that the extent of depletion-generation varies inversely with the flow rate and the H_2O level in the test gas.

These data indicate that the ambiguous carburization data generated by different investigators originate from inattention to the impurity supply rates and the inability to measure H_2O levels under about 0.1 Pa (1 μatm). We further believe that multispecimen exposures in retorts at modest HTGR flow rates cannot determine the effects of alloy composition because of the position dependence of carburization caused by impurity depletion. A further consequence of the variability of the test gas is

Fig. 5.16. Carburization of Hastelloy X at upstream positions determined by H_2O .

Table 5.11. Generation and depletion of HTGR-helium impurities in retorts at 1000°C^a

Flow rate (L/min)	Outlet minus inlet concentration difference ^b [Pa (μatm)]			
	H ₂	CH ₄	CO	H ₂ O
1.0	4.0 (39)	-3.1 (-31)	0.5 (5)	-0.1 (-1)
10	1.4 (14)	-0.6 (-6)	0.2 (2)	-0.1 (-1)
10	0.5 (5)	0.1 (1)	0.2 (2)	0 ^c

^aRetort dimensions, 3.5-cm-diam by 6-cm-long; furnace loadings 85 cm² of specimens.

^bInlet composition, 50.7 H₂-5.1 CH₄-5.1 CO-0-0.1 H₂O Pa (500 H₂-50 CH₄-50 CO-0-1.0 H₂O μatm).

^cP_{H₂O} at inlet, 1 mPa (0.01 μatm).

the carburization kinetics, which shows a time dependence of carburization ranging from $t^{1/2}$ to t^n , where $n > 1$ (i.e., accelerating). We believe that alloys positioned in retorts upstream of the maxima will show a $t^{1/2}$ dependence as a result of the oxide film-forming capacity of the test gas because the H₂O has not been completely depleted. Conversely, downstream of the maxima, the carburization rates by the H₂O-depleted test gas are governed by the availability of CH₄, as evidenced by the similarity of the carbon uptake experienced by Hastelloy X, alloy 800H, and iron (Fig. 5.14). Under limited availability, carburization is expected to increase linearly with time (i.e., $\Delta C \approx t$). Finally, the accelerated carburization rates (i.e., when $n > 1$) are expected to occur at a position in the retort where most of the H₂O and a minimum amount of the CH₄ have been depleted.

5.3.3 Effect of H₂O Supply Rates in Carburization — H. Inouye

Lupton has noted that HTGR-helium that contains 0.05 Pa (0.5 μatm) H₂O is more oxidizing and less carburizing toward alloys than is an equivalent test gas containing 0.15 Pa (1.5 μatm) H₂O if the H₂O supply rate $\dot{S}(\text{H}_2\text{O})$ in the drier gas is greater than the wetter gas.⁸ This observation (based on sparse data) has been verified by a series of tests of Hastelloy X and

alloy 800H in a test gas containing equal concentrations of H_2 , CO , and CH_4 . Variable levels of $\dot{S}(H_2O)$ were obtained by using various H_2O levels and a range of HTGR flow rates.

Table 5.12 summarizes the effect of $\dot{S}(H_2O)$ on the carburization of the above alloys after 2500-h exposures at $1000^\circ C$. Data for the runs at P_{H_2O} levels of 91 and 96 mPa (0.90 and 0.95 μatm) clearly show that a large spread in carburization is possible at equivalent gas compositions but at different flow rates. A plot of these data in Fig. 5.17 shows that the carburization of Hastelloy X varies linearly with $\log \dot{S}(H_2O)$ but that alloy 800H appears to consist of two straight lines intersecting at an $\dot{S}(H_2O)$ value of about $7 \times 10^{-7} g/(cm^2 \cdot h)$.

Fig. 5.17 is of particular importance because (1) it shows that there is no difference in the carburization resistance of the alloys in very dry gas [e.g., $\dot{S}(H_2O) = 3 \times 10^{-7} g/(cm^2 \cdot h)$]; (2) the neutral environment, which is neither carburization nor decarburization, is about $3 \times 10^{-5} g H_2O/(cm^2 \cdot h)$ for both alloys; and (3) the carburization of alloy 800H is relatively insensitive to $\dot{S}(H_2O)$ above about $7 \times 10^{-7} g/(cm^2 \cdot h)$ and would be the preferred alloy for these service conditions.

Table 5.12. Carburization versus H_2O supply rate $\dot{S}$ at $1000^\circ C$

P_{H_2O}		Helium flow ^a (L/min)	Exposed area (cm^2)	Supply rate $\dot{S}$ [$g/(cm^2 \cdot h)$]	ΔC [$\mu g/(cm^2 \cdot 2500 h)$] ^b	
(Pa)	(μatm)				Hastelloy X	Alloy 800H
0.09	0.90	10	13	3.3×10^{-5}	1,030	-270
0.14	1.40	2	10	1.3×10^{-5}	-620	-770
0.10	0.95	1	13	3.5×10^{-6}	6,125	1,410
0.02	0.2	1	16	6.0×10^{-7}	11,170	731
0.001	0.01	10	13	3.7×10^{-7}	14,130	10,860

^aHelium flow: 50.7 H_2 -5.1 CH_4 -5.1 CO Pa (500 H_2 -50 CH_4 -40 CO μatm) in 34.5 kPa (5-psig).

^bSpecimens, 6.35-mm-diam: carbon change ΔC ($\mu g/cm^2$) = $[(\Delta wt \text{ ppm})(diam)(alloy \text{ density})]/4$.

Fig. 5.17. Supply rate of H_2O governs carburization of Hastelloy X and alloy 800H.

5.4 JOINING TECHNOLOGY — G. M. Goodwin and J. F. King

Advanced Gas-Cooled Reactor system designs have called for reactor outlet temperatures of 850°C and higher, and materials screening test programs have suggested some alloys that possess elevated-temperature mechanical properties, oxidation resistance, and carburization resistance sufficient for their application in the impure helium environments of these advanced systems. These materials include cast nickel-base superalloys and mechanically alloyed oxide-dispersion-strengthened (ODS) alloys. Fabrication of the various reactor components will depend to a large extent on welding, and many of these materials are difficult to join. In this program we have examined the fusion weldability of cast alloy 713LC and ODS MA956.

The change of emphasis in the HTGR program to a steam cycle-cogeneration system has prompted an investigation of tubesheet cladding. In this program we will examine the weld cladding of a large alloy 800H forging with the nickel-base alloy ERNiCr-3.

5.4.1 Electron Beam and Laser Welding of Oxide-Dispersion-Strengthened Alloy MA956 — J. F. King and H. E. McCoy

Alloy MA956, a ferritic iron-chromium-aluminum alloy that is dispersion strengthened with yttrium aluminates, is a candidate for application as a thermal barrier seal. Preliminary joining studies of this material have been reported,⁹ and work continued during this reporting period on welding parameter studies and creep testing of weldments.

The pulsed-laser welding process was used on 1.2-mm-thick sheets of MA956. A beam power of 450 W was pulsed at 50 pulses per second to make welds at various travel speeds. Our previous work had shown that travel speed affects the weld microstructure and porosity content and that increasing speed is beneficial. Welds made at travel speeds of 4.2, 8.4, and 12.6 mm/s produced the fusion zone microstructures shown in Figs. 5.18, 5.19, and 5.20, respectively. We found that the fusion zone microstructure changes substantially with the increase in travel speed from 4.2 to

Y-179398

Fig. 5.18. Laser weld in MA956 sheet material made with 450 W, 50 pulses per second, and 4.2-mm/s travel speed.

Y-179397

400 μ m

Fig. 5.19. Laser weld in MA956 sheet material made with 450 W, 50 pulses per second, and 8.5-mm/s travel speed.

Y-179396

400 μ m

Fig. 5.20. Laser weld in MA956 sheet material made with 450 W, 50 pulses per second, and 12.6-mm/s travel speed.

8.4 mm/s but that it changes less with the increase from 8.4 to 12.6 mm/s. We also found that full penetration could not be obtained with the higher travel speed and the full power output of the laser. This situation limited our possible welding parameter variations and their effect on microstructure. The fusion zone microstructure has an influence on weldment creep properties, as will be shown later in this report.

The electron beam welding process offered us higher power capabilities for full penetration at higher travel speeds. Welding parameters were developed to produce full penetration welds in the 1.2-mm-thick sheet material at travel speeds of 21, 31.5, and 42 mm/s. The fusion zone microstructure of the three conditions are shown in Figs. 5.21, 5.22, and 5.23. Figure 5.21 shows that the weld at 21 mm/s has a fine equiaxed grain microstructure in the center of the fusion zone, similar to the laser welds. It also contains more porosity than the laser welds as previously reported. As the travel speed increased up to 42 mm/s (Figs. 5.22

Y-179395

Fig. 5.21. Electron beam weld in MA956 sheet material made at a travel speed of 21 mm/s.

Y-179394

400 μm

Fig. 5.22. Electron beam weld in MA956 sheet material made at a travel speed of 31.5 mm/s.

Y-179393

400 μm

Fig. 5.23. Electron beam weld in MA956 sheet material made at a travel speed of 42 mm/s.

and 5.23), the fusion zone microstructure changed to larger elongated grains resulting from epitaxial grain growth to nearly the weld centerline. This microstructure resulting from the higher travel speeds was found to produce the longest rupture times in creep testing.

Limited creep-rupture tests were conducted to evaluate the differences in properties of the two welding processes and to compare weldments with base metal. The results of these tests are summarized in Table 5.13. Although insufficient test data exist to make definite conclusions, they do indicate that electron beam welds have longer rupture lives than do the laser welds tested. Electron beam weldments used in these tests were made under the same conditions that governed the weld cross section shown in Fig. 5.23. A further slight improvement in rupture life was obtained from a specimen made with the electron beam impinging on the sheet at a 45° angle. This method produced a microstructure similar to that of the

Table 5.13. Alloy MA956 creep test results for laser and electron beam weld specimens

Test number	Material ^a	Temperature (°C)	Stress		Time (h)	Approximate creep strain (%)
			(MPa)	(ksi)		
22644 ^b	Base metal	760	113	16.4	1059.0	1.0
22440	Laser metal	950	69	10.0		
22407	Laser weld	950	35	5.0	0.05	
22428	Laser weld	950	14	2.0	22.5	0.7
22430	Laser weld	950	7	1.0	213.0	2.6
22511	Laser weld	760	103	15.0	0.03	
22519	Laser weld	760	48	7.0	1.7	4.9
22643	Electron beam weld	760	48	7.0	5.3	
22642 ^c	Electron beam weld	760	48	7.0	6.7	

^aGage length of specimens contained weld and base metal. All tests were conducted in air.

^bTest failed at grip.

^cWeld angled at 45° to the plane of sheet.

previous weld, but the fusion zone is angled across the sheet as shown in Fig. 5.24. This weldment failed in testing after 6.7 h at a stress of 48 MPa at 760° compared to 5.3 h for the other electron beam weldment. These rupture lives are an improvement over the laser welds that failed at 1.6 h, but all are still much shorter than those of the MA956 base material.

In addition to the reduced creep-rupture properties of the weld compared to base metal, another potential problem was observed in these welding studies. Both Figs. 5.22 and 5.23 show that some delamination of the base metal has occurred adjacent to the fusion line. It is not known whether this is a generic problem or is only associated with the particular heat on which this work was conducted.

Y-180208

400 μ m

Fig. 5.24. Electron beam weld in MA956 sheet material made with the beam impinging the surface at a 45° angle.

5.4.2 Joining of Cast Nickel-Base Alloys — J. F. King and H. E. McCoy

The objective of this program was to examine the feasibility of making autogenous electron beam welds in cast nickel-base alloys and to evaluate the weldment mechanical properties. Our previous work in joining alloy 713LC had indicated potential problems due to its hot cracking sensitivity.⁹ Plate specimens of alloy 713LC with a thickness of 10 mm were cut from an ingot and welded by the electron beam welding (EBW) process. We found that welding parameters that produce high depth-to-width ratio fusion zones were beneficial in reducing weld cracking but that cracking was not completely eliminated. We also found that cracking tended to increase as the section thickness increased.

In the effort to obtain alloy 713LC weldment specimens for testing, welding tests were made on 3.2-mm-thick plates. We were successful in producing crack-free welds in this thickness. A cross section of the EBW in 3.2-mm-thick 713LC is shown in Fig. 5.25. No evidence of cracking occurs in this cross section. Weldments of this type were used in the preparation of creep specimens.

The creep tests were conducted in air on the transverse weldment specimens, and all welds were in the as-welded condition (Table 5.14). Only four tests were conducted in this preliminary study. The alloy 713LC specimens failed in the weld metal in times lower than the base metal by a factor of about 10.

Table 5.14. Alloy 713LC creep test results for electron beam weld specimens

Test number	Temperature (°C)	Stress		Time (h)	Approximate creep strain (%)
		(MPa)	(ksi)		
22234	871	207	30	10.2	
22743	871	138	20	63.0	0.5
22232	760	414	60	64.7	
22238	760	276	60	1874.0	0.7

Fig. 5.25. Cross-sectional view of electron beam weld in 3.2-mm-thick 713LC.

5.4.3 Tubesheet Weld Cladding Program — J. F. King

A new program has been initiated to examine the weld cladding of a forged alloy 800H tubesheet. This forging, produced by Cameron Iron Works, Inc., is representative of the large tubesheet forgings anticipated for use in HTGR steam generators. Current designs require the tubesheet surface to be clad with ERNiCr-3 weld metal to permit internal bore welding of the tubes to the tubesheet. Our concern is to determine if weldability problems exist with the relatively large-grained microstructure of the alloy 800H forging.

The original forging for this work was approximately 760 mm in diameter by 457 mm thick. This has been cut at GA so that we have the full thickness and about one-half the top surface for the cladding study.

Our current plans are to clad this surface with a 13- to 16-mm thickness of ERNiCr-3 weld metal by use of the hot-wire gas tungsten arc process. After cladding, we will evaluate the cladding integrity and examine the heat-affected zone for cracking.

5.5 STRUCTURAL CERAMICS — D. R. Johnson

5.5.1 Characterization of Fibrous Insulations — D. R. Johnson

Three fibrous insulation materials manufactured by Babcock and Wilcox were characterized for possible use as HTGR thermal insulations. These materials (Unifelt 3000 board, Unifelt 2600 board, and Kaowool blanket) were characterized by spectrographic analysis, x-ray diffraction, scanning electron microscopy, ignition studies and bulk density measurements, resilience tests, helium permeation tests, and compressive stress-strain tests. Details and results of the experimental work have been published.¹⁰

Of the properties measured, resilience is probably the most important for determining the acceptability of a fibrous insulation for HTGR use. Resilience, both initially and after aging, will affect the ability to retain the material clamped between metal cover plates. Resilience of 90% or greater after 20 h at 816°C (1500°F) in a simulated HTGR environment has been chosen as an arbitrary acceptability criterion.¹¹

Specimens of Babcock and Wilcox Unifelt 3000 and Unifelt 2600 were aged for up to one year in an HTGR helium mixture at 816°C (1500°F) and were measured for resilience by a procedure described elsewhere.¹⁰ Resilience values for the specimens are presented in Table 5.15.

The data in Table 5.15 suggest that little aging effect occurs in less than 2190 h (three months) but that aging for longer times may be significant. The differences between the 20-h and 2190-h (three-month) results are not significant. The 20-h data are averaged for six specimens of each type, but the other results are for single specimens. The Unifelt 3000 insulation apparently does not compress beyond an initial 3 to 4% that occurs during the first 20 h. The Unifelt 2600 insulation does not meet the 90% resiliency criterion.

Table 5.15. Resilience of fibrous insulation specimens after aging in an HTGR helium mixture at 816°C (1500°F)

Aging time (h)	Resilience (%)	
	Unifelt 3000	Unifelt 2600
20	97.3 ($\sigma = 1.92$)	88.3 ($\sigma = 1.19$)
2190	98.0 ^a	89.2 ^a
4380	93.3 ^a	83.7 ^a
8760	96.5 ^a	85.0 ^a

^aSingle specimen.

However, even in the case of the Unifelt 3000, sufficient uncertainty exists in the magnitude of the compression (due to random experimental error) to make it difficult to conclude that there will be no continuing systematic compression with time. A small systematic compression over 20 years would, of course, be very significant.

We have examined by scanning electron microscopy the specimens of insulation aged in compression for one year. These specimens show no readily discernible degradation of the fiber morphology that would lead to a loss of resilience. The scanning electron micrographs are shown in Figs. 5.26 and 5.27. The elemental analyses by energy-dispersive x-ray analysis are shown in Figs. 5.28 and 5.29. The gold peaks result from the conductive gold coating deliberately applied to the specimens. When the two-year specimens are available, they will also be examined by scanning electron microscopy; any alteration of the fibers relative to the original material and one-year samples will then be determined.

5.5.2 Depressurization Testing — D. R. Johnson

We conducted a study to determine the effects of rapid depressurization on the integrity of structural ceramics and fibrous insulations. Tests were conducted at room temperature, which is considered to represent the worst case. The description of the apparatus and many of the experimental results were reported in last year's annual report.¹¹

M-12434

10 μm

M-12435

30 μm

Fig. 5.26. Scanning electron micrographs of Babcock and Wilcox Unifelt 3000 aged in compression for one year at 815°C in HTGR-helium.

M-12436

10 μm

M-12437

30 μm

Fig. 5.27. Scanning electron micrographs of Babcock and Wilcox Unifelt 2600 aged in compression for one year at 815°C in HTGR-helium.

Fig. 5.28. Scanning electron microscopic energy-dispersive x-ray analysis of Babcock and Wilcox Unifelt 3000 after aging for one year in HTGR-helium at 816°C.

Fig. 5.29. Scanning electron microscopic energy-dispersive x-ray analysis of Babcock and Wilcox Unifelt 2600 after aging for one year in HTGR-helium at 816°C.

More recently, three specimens of fine-grained fused silica made by Thermo Materials were subjected to the depressurization test, with no visible effects. These specimens, along with control specimens machined from the same blocks of material, were then sent to GA for measurement of diametral crushing strength.

These tests complete the planned depressurization work on potential core support structural ceramics. Several materials have been tested, including a dense 99.5% Al_2O_3 , a silicon nitride-bonded SiC , and three types of fused SiO_2 . Of these, only one of the fused silicas (Harbison-Walker Masrock, which is a low-strength material) showed any visible damage. Three of four Masrock specimens disintegrated. Diametral crushing tests by GA will determine if any structural damage was incurred by the other materials. Two types of fibrous insulation material were also depressurization tested, with neither visible effects nor significant weight loss.

All specimens tested were depressurized at ambient temperature from 7.9 MPa (1100 psi) at a maximum rate of approximately 53 MPa/s (7700 psi/s) over a 1.4-MPa (200-psi) pressure drop. The average rate over a 4.8-MPa (700-psi) pressure drop was about 39 MPa/s (5600 psi/s). This result compares with a postulated HTGR-gas turbine depressurization rate of 43 MPa/s (6300 psi/s) following a turbine-deblading accident.

5.5.3 Carbon Composite Core Support Pads — A. J. Caputo

The objective of this program was to develop a carbon fiber composite plate with the necessary compressive strength, thermal insulation, and thermal stress properties for use as High-Temperature Reactor (HTR) core support pads. The overall fabrication process included

1. carbonizing of chopped rayon fibers,
2. forming flat plates by vacuum molding a water slurry of these fibers,
3. cold pressing the plates, followed by drying and curing while under compression, and
4. sintering (without pressure) the cured plates.

Initially, the effect of the carbonization cycle was investigated. Rayon fibers were carbonized at maximum temperatures of 900 to 1350°C and at total cycle times from 8 to 24 h but with one test at 100 h. The nominal coke yield was 80% and was not significantly affected by this range of conditions. Similarly, fibers from the various cycles did not show appreciable differences in scanning electron microscopic examination or subsequent processing. After molding, plates were dried and cured both as-molded and as cold-pressed. Specimens cold pressed at 3.6 MPa (520 psi) produced a sintered density of 0.51 g/cm³ and a compressive strength (at 5% deflection) of 15.4 MPa (2210 psi). This result is probably sufficient for this application. By comparison, the density and strength of as-molded (without cold pressing) specimens were 0.19 g/cm³ and 0.83 MPa (120 psi), respectively. The increase in compressive strength and increases in sintered density are shown in Table 5.16. Unfortunately, the determination of the thermal conductivity of the above specimens was discontinued because of decreased funding. However, we know of no technical reason why specimens with the necessary conductivity cannot be fabricated. The most critical unanswered question is the performance of the material in long-term (20–40 year) use under a vibrating load. Testing aimed at this characteristic was outside the scope of this investigation but should be considered if interest in this type of material continues.

Table 5.16. Density and compressive strength of carbon-carbon core support materials

Specimen type	Sintered density (g/cm ³)	Compressive strength (at 5% deflection)	
		(MPa)	(psi)
As-molded	0.19	0.83	120
Cold-pressed	0.33	3.9	560
Cold-pressed	0.44	9.8	1420
Cold-pressed	0.51	15.4	2210

5.5.4 Characterization of Dense Ceramics — R. J. Lauf

The purpose of this activity is to determine the microstructural details of candidate structural ceramics for the HTGR core support pads. Coupled with phenomenological creep data (Sect. 5.2.5), microstructural information will strengthen our understanding of the fundamental creep processes expected in an operating core support structure. Identification of creep mechanisms is necessary to extrapolate accelerated creep test results realistically enough to predict long-term behavior.

5.5.4.1 Fine-Grained Thermo Materials Fused Silica

This material was characterized by x-ray diffraction, transmission electron microscopy (TEM), optical microscopy, and mercury porosimetry. The results are summarized below.

1. The microstructure consists of large grains of glass, surrounded by finer material containing some porosity.
2. X-ray diffraction indicates the material to be completely amorphous, with no detectable crystalline phases.
3. Transmission electron microscopy shows that the larger grains are amorphous, but indicated a trace of crystalline material in the fine constituent. The crystalline phase has not yet been identified.
4. Quantitative measurement by mercury penetration of porosity seen in the TEM photos gives about 9 vol % open porosity with an average pore size of about 0.2 μm .

Specimens of Thermo Materials fused silica will be examined after creep testing and will be compared to the as-received material to determine the dominant creep mechanisms and to predict the microstructural changes (if any) that might occur in service.

5.5.4.2 Silicon Nitride

Silicon nitride type NCX-34 ($\text{Si}_3\text{N}_4 + \text{Y}_2\text{O}_3$) was extensively characterized in the as-fabricated condition, and the results were reported previously.¹² A four-point bend test specimen of NCX-34 was removed after 3% creep strain at 1010°C and sectioned so that TEM specimens were obtained from both the tension and compression sides.

Examination of the area that had undergone 3% tensile creep strain showed several microstructural changes:

1. Some elongated grains were present after creep, but the as-received material contained primarily equiaxed grains.
2. Dislocations were more numerous after creep, and the formation of subgrains appeared to be a mechanism for large grains to accommodate creep strain.
3. Some microcracks appeared to be opening up in the direction of tensile strain.

Examination of the area that underwent 3% compressive creep strain showed the following changes compared to the as-fabricated material:

1. Some elongated grains were present. Numerous dislocation networks were observed, leading in some cases to the formation of subgrains.
2. No microcracks were observed, which was not surprising in view of the compressive stress state.

Examination of the surface of the test bar in an unstressed area was done by scanning electron microscopy to investigate the possibility of surface attack by the high-temperature atmosphere. The micrographs showed no evidence of chemical alteration or attack or removal of grain boundary phases. The only observed changes were associated with growth of the Si_3N_4 grains. This grain growth was previously observed by TEM.

5.5.4.3 Alumina

Two grades of alumina are being considered for portions of the HTGR core support. Creep testing and microstructural characterization of AD-85 and AD-995 aluminas will begin in early 1982. A full-size core support pad of AD-85 has been received from GA and will be photographed and sectioned to provide samples from several areas of the pad. This action will allow us to evaluate the homogeneity of the pad and to detect any anisotropy in the overall structure.

Smaller samples of AD-995 are expected soon. These samples, as well as those cut from the AD-85 pad, will be characterized by the techniques discussed earlier, before and after creep testing. This testing will provide a better data base for future design decisions.

5.6 FISSION PRODUCT-MATERIALS INTERACTIONS — H. E. McCoy and C. R. Brinkman

The basic approach being used to expose samples of Hastelloy X, Inconel 617, and other materials is shown in Fig. 5.30. The oxygen content of the helium carrier gas is fixed by passing the gas over hot chromium and Cr_2O_3 . Cesium vapor is added by bubbling the gas through liquid cesium at a controlled temperature, and tellurium vapor is added by positioning solid tellurium at the proper temperature in the furnace. The basic problem encountered in our approach is that the test samples comprise only a small portion of the total surface area in the furnace chamber. Several modifications have been made in the testing equipment, and tellurium is being deposited on the test samples in controlled amounts. Some problems still remain with the deposition of cesium.

Samples of Hastelloy X and Inconel 617 formed intergranular cracks when exposed to tellurium in the gas exposures much as noted previously when these materials were exposed to small partial pressures of tellurium in a fluoride salt environment.¹³

Fig. 5.30. Schematic of apparatus for exposing samples to helium containing small amounts of tellurium and cesium.

5.7 LINER AND PENETRATION STUDIES — D. O. Hobson and R. K. Nanstad

Because it has been several years since the goals of the liner and penetration studies have been described in this report series, it is appropriate to reiterate the purpose and objective at this point.

The several thick-section steel members that constitute the penetrations and closures of the PCRV are governed by rules for fracture toughness defined by Division 1, Sect. III of the *ASME Boiler and Pressure Vessel Code*.¹⁴ Those toughness requirements were developed, however, for

materials used in light water reactor (LWR) pressure vessels. Because the PCRV steels differ from LWR steels, a need exists for developing realistic requirements for the PCRV ferritic materials and applications. Therefore, the objectives of the program are

1. to define a reference fracture toughness curve for ferritic components of the HTGR primary pressure boundary similar to the K_{IR} curve in Appendix G of ref. 14,
2. to define a temperature-indexing procedure for this curve, based on standard materials qualification tests [e.g., Charpy V-notch (C_V) and drop-weight] similar to the RT_{NDT} procedure of ref. 14,
3. to obtain a quantity of fracture toughness data large enough that a statistically meaningful evaluation of the data can be conducted to serve as a tool to help assess the probability of component failure.

During the last several years, various heats of plate and forging material were tested; for the past two years, weldments of various combinations of those materials have been tested. During this past year we have continued the weldment tests. The testing program includes C_V and drop-weight impact, subsize and standard [12.8-mm (0.505-in.)] tensile and compact specimens, as well as instrumented precracked Charpy specimens for static and dynamic fracture toughness. Most of the past year's work consisted of data gathering from the test procedures mentioned. Some analyses have been made, however, to evaluate the quality of the data, particularly the drop-weight results.

The previous annual report in this series presented results of precracked Charpy V-notch (PCVN) testing of the W-5 weldment weld metal, which showed little variation in fracture toughness behavior with position in that weld metal. Additionally, there was no evidence of carbon diffusion into the root region. There was, however, considerable scatter in the data so that a lower bound curve was deemed appropriate.

Specimens of the W-5 weldment were drop-weight tested, and the resultant nil-ductility temperature (NDT) of -70°C can be compared with the -80°C NDT of weldment W-3. In addition, drop-weight tests conducted on the W-1 metal produced an NDT of -85°C . It was previously found and reported that the W-3 NDT of -80°C was not equal to RT_{NDT} because the two specific ASME requirements¹⁴ were not met. Three C_V tests conducted at

a temperature not greater than $NDT + 33^{\circ}C$ ($60^{\circ}F$) must each exceed (1) 68 J (50 ft-lb) absorbed energy and (2) 0.89 mm (0.035 in.) lateral expansion. The W-3 weldment did not meet these requirements.

Scanning electron microscopic examination of the W-3, W-5, and W-1 fracture surfaces showed a ductile heat-affected zone (HAZ) between the base weld metal and the Hardex-N crack starter weld. This result is illustrated in Figs. 5.31 and 5.32 for the W-3 weldment. The left sections of both figures are the same and show, at 100X, the approximate boundary between the Hardex-N HAZ and the base weld metal. The rectangles outline the areas shown in the right portions of the figures at 1000X. The base weld metal is toward the top of the left areas. In Fig. 5.31 the outlined area is comprised primarily of cleavage-type fracture, which is expected for the weld metal at the $-85^{\circ}C$ test temperature. Conversely, Fig. 5.32 shows large areas of dimple-type ductile fracture, which is

M-16634

Figure 5.31. Scanning electron micrograph of W-3 heat-affected zone showing brittle base weld material (small rectangle at left encloses area shown enlarged at right).

Fig. 5.32. Scanning electron micrograph of same W-3 heat-affected zone area shown in Fig. 5.31. Small rectangle encloses ductile heat-affected zone material shown enlarged at right.

anomalous for the weld metal and for the Hardex-N. We feel that any ductile region between the Hardex-N and crack starter weld and the body of the weldment to be tested can cause an artificial lowering of the NDT and thereby create a nonconservative estimate of fracture toughness. This behavior has been confirmed by recently published work by the Japan Steel Works, Ltd.,¹⁵ which showed that the two-pass procedure recommended by ASTM for drop-weight testing ameliorates the toughness of the HAZ (where the beads overlap) and gives randomly lower values of NDT. New drop-weight specimens are being machined and will be tested with a one-bead Hardex-N crack starter weld for comparison.

The W-3 plate material (SA 537, class 2) was tested with 1T compact specimens to complete the study of the W-3 weldment. The results showed an upper shelf toughness K_J of 500 to 550 MPa $\sqrt{m}$ (450–500 ksi $\sqrt{in.}$) at

-50°C (-58°F) and a transition region from about -125°C (-193°F) to -50°C (-58°F). The toughness K_J at -150°C (-238°F) was $31 \text{ MPa } \sqrt{\text{m}}$ ($28 \text{ ksi } \sqrt{\text{in.}}$). The data are shown in Fig. 5.33.

The W-1 weldment consists of 89-mm-thick (3-1/2-in.) plates of SA 537, class 1, steel joined by 90% submerged arc weld (SAW) and 10% SMAW techniques. In addition to the W-1 drop-weight specimens discussed earlier, both Charpy and 1T compact specimens were tested. The Charpy specimens from the HAZ have been tested as PCVN specimens and gave the results shown in Fig. 5.34. Specimens were taken from positions near the weld root and from the 1/4 wall position. The transition occurred at approximately -60 to -65°C and the upper shelf K_J value was approximately $330 \text{ MPa } \sqrt{\text{m}}$ ($300 \text{ ksi } \sqrt{\text{in.}}$). No differences were found in the toughness at the two specimen positions.

Compact specimens (1T) were machined from the weld metal of the W-1 weldment, and unloading compliance tests are being performed to produce full J -integral versus crack extension resistance J - R curves. Such curves give a graphical indication of specimen toughness relative to crack initiation and tearing resistance. Three specimens have been tested to

Fig. 5.33. Fracture toughness of W-3 plate material.

Fig. 5.34. Fracture toughness of the W-1 weldment heat-affected zone.

date at RT , -20 , and -40°C . The room-temperature curve was run with 45 unloadings (15% decrease in load) and the -20°C curve with 25 unloadings. (Data from the -40°C specimen, which failed after the third unloading, had no appreciable stable crack growth.) Enough tests will be run to obtain the complete J_{Ic} versus temperature curve, by both the unloading compliance method and the energy to maximum load method as applicable.

The question of lower bound toughness relative to thick-section structural behavior is of great interest in the technical community, and many efforts are under way to determine a method for analyzing elastic-plastic fracture toughness data in the transition region so that a lower bound toughness value is obtained. One method being used is the β_{Ic} correction of Irwin.¹⁶ The correction is based on a relationship between the plastic zone size and the thickness of the specimen. The technique may be useful in adjusting K_{Ic} values, which are elevated above K_{Ic} because of plane-stress yielding when more accurate K_{Ic} values (such as from large specimens) are not available. The correction has been applied with success to some of the fracture toughness data obtained previously for the base materials (F1, P1, P2, and P4).

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6. GRAPHITE DEVELOPMENT

W. P. Eatherly

6.1 CAPSULE IRRADIATIONS — J. A. Conlin

6.1.1 Creep Capsule OC-4 — R. L. Senn

Irradiation of capsule OC-4, the fourth in a series of graphite creep irradiation experiments, began on November 25, 1980, in core position E-5 of the Oak Ridge Research Reactor (ORR). The capsule was removed from the ORR on August 20, 1981, after completing its scheduled irradiation period of 115 d.

Capsule OC-4 contained graphite specimens previously irradiated in capsule OC-2 to a fast fluence of 2×10^{21} neutrons/cm² ($E > 0.18$ MeV) at 600°C specimen temperature and with compressive stresses of 13.8 and 20.7 MPa being applied to the stressed specimens. The objective of capsule OC-4 was to extend the exposure of the test graphites to $>4 \times 10^{21}$ neutrons/cm² ($E > 0.18$ MeV) under the operating conditions established for the first 600°C experiment (OC-2). Detailed design descriptions of the graphite creep irradiation experiments have been published.¹⁻⁵

Except for the previously reported⁵ excessively high-temperature operation (and consequent operation of the reactor at 27 MW) and the minor leak that was discovered in the containment gas system (which neither increased during the last month of operation nor affected experiment operation), the experiment operated satisfactorily and met the established experiment goals. Capsule OC-4 was operated with specimen temperatures of 600^{+30}_{-10} °C, and a compressive stress of 13.8 MPa (20.7 MPa for selected specimens) was applied to the graphite specimens. During the experiment, two of the thermocouples in the graphite holder failed; however, spare

thermocouples were available in the same region of the experiment, and operation of the experiment was not impaired. The movable thermocouples completed over 98 traverses and operated satisfactorily, except for the final 2 d of operation when the south thermocouple began to fail as it traversed the lower region of the experiment. This incipient failure did not affect experiment operation either.

The irradiation of capsule OC-4 had such a checkered career that, for future reference, we detail its history. Soon after bringing the reactor to full power operation (30 MW), it became obvious that the upper regions of the capsule would operate at temperatures above the desired 610°C maximum specimen temperature. Capsule OC-4 was designed to operate with 100% He in the gas gap between the outer sleeve and the outer capsule containment vessel. Electrical heaters were provided to bring the specimen temperatures to the design point. The heaters were arranged in ten 50.8-mm-long (2 in.) zones along the length of the specimen columns. We found that, as the reactor cycle proceeded and the control rods were retracted in the normal course of reactor operation, the upper three zones required less and less heater power. When the required zero heater power was reached, there was no other way to control the experiment temperature except by a reduction in reactor power. With the concurrence of Reactor Operations and other experimenters, we therefore requested a reactor power level of 27 MW. The experiment could be controlled satisfactorily at this power level for most of a refueling cycle (approximately 10 d). Even at 27 MW, near the end of the fuel cycle, the upper zones exceeded the design temperature and reached approximately 630°C average specimen temperature in zone 2 during the last day of a fuel cycle.

The unexpected high temperatures in the upper zones of capsule OC-4 were probably caused by a combination of factors: (1) The reactor core loading was significantly different from that used when capsule OC-2 was operated. The existing core-loading patterns were designed to accommodate the needs of the Magnetic Fusion Energy Program experiments then in operation. (2) New control rods had been installed near CP-E5; as described previously, the OC-4 experiment seemed to be especially sensitive to the control rod positions, as evidenced by the increasing

temperatures near the end of a fuel cycle. (3) Although the design of capsule OC-4 was essentially the same as that for capsule OC-2, slight differences might have exacerbated the tendency for the experiment to operate at excessive temperatures. Because of the unavailability of AFX-type graphite, Stackpole grade 2020 graphite was used for the specimen holder and sleeve. We encountered great difficulties in attempting to install the close-fitting graphite sleeve over the specimen holder. To assemble OC-4, we were forced to increase the inside diameter of the graphite sleeve slightly. This increase should have had a minimal effect on capsule operation but, nevertheless, would have accounted for some increased thermal resistance through the experiment to the coolant water.

In all other respects, capsule OC-4 operated satisfactorily. Temperature profiles were measured each working day with the movable thermocouples, and temperatures were adjusted accordingly to maintain the design $600 \pm 10^{\circ}\text{C}$ specimen temperatures for all zones except the upper three. Pressures were maintained within design limits on the bellows system used to compressively stress selected specimens. Routine tests have been conducted to date with the pneumatic electromagnetic (PEM) sensor cells located at the bottom of each stressed column. These tests have shown that each column was indeed being compressed by the upper bellows system and that there were no cocked or jammed specimens in the columns that might compromise the application of the stress to all the specimens.

During early January a conflict arose between the OC-4 requirement for 27-MW reactor operation and the several experiments being operated by the Fusion Energy Materials Program. Although temperatures could generally be maintained near the desired design points for all experiments, questions on flux effects could be raised. We therefore decided to remove capsule OC-4 from the reactor until summer, when reactor operation at the lower power level generally was mandated by weather. Accordingly, capsule OC-4 was removed on January 15, 1981, and stored at poolside for future reinsertion. Obviously, the manhandling of this complex capsule required in removal and reinsertion in the core was not undertaken lightly.

Unfortunately, thermocouple and other problems led to the removal of the Fusion Energy capsules prematurely, and on February 4, 1981, OC-4 was again inserted in the core. On March 16, the Fusion Energy experiments were resumed, and capsule OC-4 was again removed. At this point OC-4 had attained 85 d of the scheduled 110-d irradiation.

As the capsule was placed at poolside for storage, a fine stream of bubbles was observed to be escaping from the upper end of the in-core section of the capsule. Closer inspection revealed what appeared to be a pinhole leak in the vicinity of the transition piece weld that joins the in-core portion of the capsule to the upper lead tube going to the reactor vessel upper-face entry flange. Because the capsule had always been under pressure, inleakage of water was most unlikely. Attempts to measure the pressure dependence of the flow to establish the leak character were unsuccessful because of the smallness of the leak and the crudity of the pressure measurements. It was, however, established by collecting the escaping gas that the leak rate at 210 kPa (30 psig) system pressure was about 30 mL STP per hour. Hot cell examination at the reactor, including photography, dye-penetrants, and additional leak measurements, implied that the leak was indeed a pinhole and was almost certainly not a crack subject to propagation. Failure of the capsule would not endanger the reactor, although it would lead to significant economic penalties for the experimental program. We therefore decided in meetings with the ORNL Reactor Experiment Review Committee to reinsert the capsule without attempting repairs. We had little hope of success for such remotely performed repairs. We further decided to monitor the leak rate and to establish a 315-kPa (45-psig) minimum capsule pressure as a reactor scram to further ensure safety of the reactor and, more significantly, to protect the samples from water ingress and concomitant oxidation.

Accordingly, OC-4 was reinserted in the reactor on July 20, 1981, and irradiation continued until its planned termination. However, it was necessary to reduce the power still further (25 MW) to preserve temperature control at $600 \pm 10^\circ\text{C}$, a situation totally unacceptable for other experiments in the core. Finding an explanation for our temperature problems became imperative, not only for OC-4 but for any future experiments in the creep series.

It was not difficult to find the problem. The need to drop to 25-MW power occurred on fuel core loading cycle 158. This particular core was configured to accommodate Fusion Energy's flux-tailing experiments, and specifically resulted in very heavy fuel loadings on the north side of the reactor core. We believed that this would probably result in an increased gamma field at the OC-4 position (E-5), but only after the fact. On July 27, 1981, a fuel reshuffling was deliberately made back to a core configuration more nearly resembling that for which the OC series was designed, and immediately power could be maintained at 27 MW or higher.

Just before termination of the OC-4 irradiation on August 20, 1981, data were taken with no electrical heating in the capsule and with a pure helium atmosphere. The reactor power level was calibrated. After refueling, the same experiment was performed with OC-4 still in place but with the core shifted to the type required by the Fusion Energy irradiation program. These experiments confirmed both the effective change in gamma heating and the 76-mm vertical upward shift of peak heating with the change in control rod position between the beginning and the end of a cycle.

After removal from the reactor and a brief cooldown period, capsule OC-4 was moved to a hot cell and disassembled. All the graphite specimens were recovered intact and transferred to the Metals and Ceramics Division for further analysis. Nineteen of the twenty flux monitor capsules that were installed in the capsule were recovered. The hot cell operators had difficulty in removing the flux monitor tubes from their positions in slots machined in the graphite specimen holder, and one monitor was lost in an attempt to pry it out. Many of the flux monitor tubes were severely bent during manipulations required to remove them. This may have caused difficulty in removing the various flux wires from the tubes when an attempt was made to analyze the wires. Efforts were made to adjust the experiment design to alleviate this problem for future experiments; however, assembly of the next experiment was too far advanced to make corrective adjustments for the next irradiation.

6.1.2 Creep Capsule OC-5 — R. L. Senn

Capsule OC-5, the fifth in a series of graphite creep irradiation experiments, was installed in core position E-5 of the ORR on

December 14, 1981. Reactor startup was achieved December 17, 1981. As of midnight December 31, 1981, the experiment had been irradiated for 11 d of a scheduled minimum 110-d irradiation. Capsule OC-5 contains specimens previously irradiated in capsules OC-1 and -3. The specimens were irradiated at 900°C specimen temperature while under compressive stresses of 13.8 MPa (20.7 MPa for selected specimens).

Completion of construction and installation of capsule OC-5 was significantly delayed from the original planned dates for a variety of reasons. During the construction period, a seven-week strike of the labor forces essentially stopped progress. Also, as noted in Sect. 6.1.1, the unexpectedly high-temperature operation of capsule OC-4 prompted revision of certain parts of OC-5, which changed the gas gaps to adjust the thermal conductance to a more suitable value. A further design change involving the movable thermocouples was also incorporated into capsule OC-5.

The modifications in design of capsule OC-5 were straightforward. As described in Sect. 6.1.1, toward the end of operation of OC-4 one of the movable thermocouples was beginning to show signs of failure. Although steady improvements have been made in the design of these movable thermocouples since capsule OC-1 (the latest operated in excess of 98 traverses), they continued to show signs of failure after extended use. We decided to incorporate a new design into OC-5 before construction was completed. This decision was influenced by recent work on capsule OC-X (the 1200°C experiment), which will require a removable thermocouple with a longer stroke. Consequently, an assembly using a coiled thermocouple wire located at the top of the thermocouple column was employed. This eliminated the formerly used sliding contact on a gold-plated board. This sliding contact was suspected of being the basic cause of failure in the former design.

The capsule assembly was completed and shipped to the ORR on November 12, 1981. During the next few days, the aluminum core piece was fitted to the capsule, and the assembly was placed in the ORR mockup facility. The proper elevation of the experiment was established, and the flange on the upper end of the experiment was welded into place. On the morning of November 17, the experiment was removed from the mockup and

transported to the ORR for installation in the reactor. At this time we noticed that the experiment had been damaged. A bend in the outer containment of the in-core portion of the capsule was clearly visible. The containment can was bent in a westerly direction at the reactor midplane level approximately 3 mm (0.119 in.) from its nominal position.

A review of events during the handling of the experiment in the mock-up and a reenactment of events with a dummy capsule at the mock-up site showed that the bending probably occurred during the attempt to remove the capsule from the mock-up. While lifting out the experiment with the overhead crane, the core fuel element mock-up bound to the capsule and lifted with it. There was apparently an attempt to lower both into the mock-up assembly, thus relaxing the hoist and permitting the weight of the capsule to sag against the edge of the core piece. Since the containment vessel is only 1 mm (0.040 in.) thick, not much force would be required to bend it.

The capsule was subsequently returned to the assembly area at Y-12. The lower containment can was removed to permit inspection of the internal parts of the experiment. A somewhat jagged, but not fragmented, circumferential crack was found around the graphite sleeve surrounding the heaters at the reactor midplane elevation. The specimens were removed from their respective positions, and inspection of the internal portion of the graphite specimen holder revealed that it, too, was cracked through at approximately the reactor midplane elevation.

Fortunately, the cracks in the graphite pieces were clean breaks with no chipping or crushing. Since the heater coils and thermocouples installed in the graphite specimen holder tend to hold the two sections together securely and the outer graphite sleeve is a tight fit over the wound heater coils, the cracked sections were in close contact. There was no serious discontinuity on the inner surfaces of the cylindrical holes that contain the specimens and their spacers. It is important, of course, that the spacers be able to move smoothly up and down the holes as the specimen column moves because of expansion or contraction due to either the imposed compressive stresses, thermal effects, or neutronic effects. Since detailed inspections had shown that the surfaces were adequately

smooth and that the cracked areas were unlikely to cause binding of the stressed columns, we elected to reassemble the capsule, leaving the cracked graphite pieces in place.

Another aspect that we considered regarding the feasibility of operating the experiment with the cracked graphite pieces was the various thermal effects that might result from the discontinuities in the graphite sleeve and holder. Again, fortunately, the cracks occurred in the center of the sleeve. This region is a 20-cm (8-in.) length with the same gas gap and is located near the midplane of the reactor flux. Therefore, the temperature is relatively constant along this length, even without heater control. Since the experiment is operated with a constant temperature along the entire length of the specimen column, there is essentially no temperature gradient across the cracked region, and the discontinuity should have little effect on heat transfer in the axial direction. If the cracks separate a small amount and are aligned just right, it is possible that the 900°C specimen might "see" the outer can, with a consequent transfer of radiant heat. However, the area involved would be very small, and the localized increased heat flux that might result would be small compared to the total heat flux across the outer can wall to the coolant water. Such an effect would, in any event, be sensed by the traveling thermocouples.

The experiment was reassembled with a new lower containment can and lower cap and was tested and returned to the ORR for fit-up and installation. New procedures for handling the experiments were prepared by the Operations Division to preclude similar incidents in the future. As noted above, installation into the reactor was accomplished on December 14, 1981.

Although the experiment continues to operate satisfactorily, several problems were encountered during the first few weeks of operation. Shortly after irradiation began, a leak was detected in the west PEM cell bellow that was used to check the forces being applied by the upper west bellows to the specimen stack. A few days later the east PEM cell bellow also appeared to be leaking. We subsequently confirmed during a refueling shutdown on December 28, 1981, that there were, indeed, large leaks in each of these systems. The design pressure in these systems could not be

maintained; therefore, the low-pressure alarms and control instrumentation built into these systems were disabled and permitted to operate at ambient pressure of the capsule control gas. Although these failures do not affect experiment operation, they do prevent ongoing measurements from showing that the compressive forces are being applied to the entire specimen column during the course of experiment operation. Except for one column in the first creep experiment (OC-1), no misalignment and concomitant binding of the specified columns has been experienced.

Four open thermocouple circuits have been detected since the experiment startup. Fortunately, all these open-circuited thermocouples have been in different heater zones so that the other thermocouple in each zone can be used as the sensor for temperature control in that zone. A batch of thermocouples from a new supplier was used for capsule OC-5; however, the new material was procured under the same specifications and quality assurance standards as the previous material. A detailed investigation is under way in an effort to determine if there is a reason for this abnormally high number of thermocouple failures so early in an irradiation period. As noted above, since each of these thermocouples has a spare, there was no adverse effect on experiment operation.

In all other respects, capsule OC-5 continued to operate satisfactorily. The specimen temperatures were being maintained at an average of 900^{+20}_{-10} °C as determined each working day by traverses of the two movable central thermocouples. Actual specimen temperatures are held very close to the 900°C design point except for zone 2, which tends to operate somewhat higher when the adjacent zones are maintained at 900°C. The movable thermocouples, which incorporate the design improvements described above, have continued to operate satisfactorily after about 20 traverses.

6.1.3 Design of 1200°C Creep Capsule OC-X — H. C. Roland,* Amir Mobasheran,* and R. L. Senn

Design studies were completed during this reporting period for graphite creep irradiation experiment OC-X, to be operated at 1200°C

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specimen temperature with compressive stresses of 13.8 and 20.7 MPa being applied to the specimens. The results from these design studies have been reported.⁶

Detailed design work was begun on this experiment. An initial design review meeting was held in late August to discuss the required features with the designers. Several important differences between the high-temperature capsule and previous OC capsules were noted: the need to control the experiment temperatures with gamma heating and gas mixture changes only (electric heaters cannot be used in the high-temperature design); incorporation of a tungsten sleeve that will serve as a gamma heat susceptor and a radiant reflector; and extended travel for the movable thermocouple. Because the movable thermocouple must be designed for 1200°C operation, it will probably be of materials that are degraded by neutron irradiation, and, hence, when measurements are not being made, it must be moved out of the high-radiation zone to extend its useful life.

Another interesting feature required for capsule OC-X operation results from the need to move the capsule vertically up to 76 mm while it is installed in the reactor and the reactor is operating. To maintain the specified 1200°C temperature over the entire length of the specimen column, it is necessary to raise the capsule in small steps during a two-week fuel cycle, following the observed shift in the peak gamma heat and thus maintaining the same relationship for peak gamma heat production and the experiment centerline over the entire cycle period. A conceptual design for this feature was completed, as well as a design for the new movable thermocouple. This new design was actually employed in capsule OC-5.

Further work on the design and construction of this capsule was postponed indefinitely at the end of fiscal year 1981 because of budgetary uncertainties.

6.1.4 Graphite Irradiation in HFIR — R. L. Senn

The High-Flux Isotope Reactor (HFIR) target capsule HTK-4, containing 48 specimens of various German graphites and two graphite holders loaded with SiC specimens as supplied by the General Atomic Company (GA), was inserted into HFIR outer target position B-1 on August 17, 1980, for a

ten-cycle irradiation. The experiment was designed to operate at $715 \pm 25^\circ\text{C}$. The capsule was subsequently removed from the reactor on April 9, 1981, as scheduled after a 21,800 MWd irradiation. Assuming a peak fast neutron flux of 1.18×10^{15} neutrons/($\text{cm}^2 \cdot \text{s}$), the experiment received a peak fluence of 2.2×10^{22} neutrons/ cm^2 ($E > 0.18$ MeV). Results from this experiment are reported elsewhere in this section.

The HFIR target capsule HTK-5, containing 64 graphite specimens, was completed and inserted into HFIR outer target position B-1 on December 24, 1981, at the beginning of HFIR cycle 218. This HTK-5 capsule is a Federal Republic of Germany (FRG) priority experiment containing five FRG experimental graphites in comparison with H451 and POCO AXF graphites. The design is similar to the previous four capsules in this series. Graphite specimens in HTK-5 will be exposed to expected lifetime fluences of 3.8×10^{26} neutrons/ m^2 (15 HFIR fuel cycles) at 600°C .

The HFIR target capsule HTK-6, also containing 64 graphite specimens with the same design as the previous capsules in this series, was designed and all parts were fabricated. Assembly was scheduled for completion in early January 1982. The HTK-6 is an HTGR priority experiment containing grades of H451 from two lots, an improved H451 and TS1621 in comparison with selected FRG grades. Graphite specimens in HTK-6 will be exposed to expected lifetime fluences of 1.8×10^{26} neutrons/ m^2 (seven HFIR fuel cycles) at 900°C . Installation of HTK-6 into the reactor has been postponed indefinitely, pending programmatic budgetary decisions.

6.1.5 Creep of Graphite Under Irradiation — C. R. Kennedy

The specimens from graphite creep capsule OC-4 (600°C) have been removed and evaluated. These specimens have accumulated an additional 2.5×10^{25} neutrons/ m^2 ϕ_G [graphite damage fluence ($E > 50$ keV)] for a maximum of 4.5×10^{25} neutrons/ m^2 ϕ_G . Compressive creep strains of up to 2% have been observed. Dimensional measurements, sonically derived elastic constants, electrical resistivities from eddy-current measurements, and 500°C coefficient of thermal expansion (CTE) measurements have been made on both the control and compressively stressed specimens. Graphite grades H451, H327, and AXF were irradiated, with H451 comprising 44 of the 54 total specimens.

Compressive creep curves are given in Figs. 6.1 and 6.2 for the three grades. The creep curves can again be described in general by an initial primary creep, followed by a second steady stage of creep. However, the second stage of creep does tend to have a decreasing rate in agreement with past results from OC-1 and -3 at a 900°C test temperature. The density increase with compressive creep is shown in Figs. 6.3 and 6.4. This increase in density was shown to be a result of primary creep in OC-1 and -3 and is completely recoverable when the stress is removed. After primary creep is saturated, the density change of the stressed specimens is equal to the controls, meaning that secondary creep occurs at constant volume. The magnitude of the density increase is directly related to the magnitude of the primary creep and, thus, to the initial elastic strain, as shown in Fig. 6.5. There does not appear to be a temperature dependence of the densification because the 600 and 900°C results are in very good agreement. The most interesting result of this study

Fig. 6.1. Compressive creep curves from OC-2 and -4, grade H451, at 600°C.

Fig. 6.2. Compressive creep curves from OC-2 and -4, grades H327 and POCO AXF, at 600°C.

Fig. 6.3. Density changes in grade H451 graphite due to creep.

Fig. 6.4. Density changes in grade H327 graphite due to creep.

Fig. 6.5. Density change due to primary creep as related to the initial elastic strain.

is that the magnitude of the densification is so large ($>5\times$) compared to the initial elastic strain. The implication is that primary creep has a Poisson's ratio that actually is negative. One might expect Poisson's ratio for primary creep to be equal to zero, but a negative ratio is hard to understand or to explain.

The 600°C reduced creep rates are given in Fig. 6.6 as a function of the initial Young's modulus in comparison with the 900°C results. As before, the creep rate coefficients are linear functions of the modulus and have parallel slopes. This situation implies that the creep process is not a thermally activated process, which requires that the creep rate coefficients have a constant ratio. This behavior supports the Cottrell mechanism of stress-directed shearing of anisotropic growth.

The elastic constants are derived from longitudinal and transverse wave velocity measurements made in the axial direction of each specimen. Both Young's modulus and the shear modulus are calculated by assuming isotropy. The results are given in Figs. 6.7 and 6.8 for grades H451 and H327, respectively. Also included for comparison are the moduli values

Fig. 6.6. Creep coefficients of graphite at 600 and 900°C as related to the initial Young's modulus.

Fig. 6.7. Effect of fluence with and without creep on the elastic moduli graphite grade H451.

Fig. 6.8. Effect of fluence with and without creep on the elastic moduli graphite grade H327.

from experiments OG-1, -2, and -3. Clearly the creep deformation does not alter the elastic constants. It is also satisfying to observe the agreement between the OG-1, -2, and -3 irradiations and the current OC series. The sonic attenuation is also measured to yield quantitative information about the defect size in the graphites. These results are given in Fig. 6.9 for H451. It is clear that the background defect size in these graphites is reduced by irradiation growth and that the creep strain has virtually no effect.

The electrical resistivity of each specimen is determined by the use of eddy-current measurements. The results are given in Figs. 6.10 and 6.11 for H451 and H327, respectively, again showing virtually no effect of creep strain on the electrical resistivities. Although electrical and thermal conductivities are controlled by separate scattering mechanisms, the implication is that, if the electronic conductivity of graphite is not affected by creep strain, the thermal conductivity is also likely to be unaltered.

Fig. 6.9. Effect of fluence on sonic attenuation with and without creep for graphite grade H451.

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Fig. 6.10. Effect of fluence on electrical resistivity with and without creep for graphite grade H451.

Fig. 6.11. Effect of fluence on electrical resistivity with and without creep for graphite grade H327.

The CTE in polycrystalline graphite is generally much lower than can be calculated from single crystal behavior because of very fine microcracks parallel to the layer planes within the individual particulates. As graphites are irradiated, the differential growth (*c*-axis expansion and *a*-axis contraction) tends to close these microcracks and to increase the coefficient of thermal expansion. As shown in Fig. 6.12, the CTE measured from room temperature to 500°C is additionally increased by creep deformation. This increase is also shown in Fig. 6.13, where the effect of creep strain is shown to continue increasing the CTE up to 2% creep deformation. We make the following specific conclusions:

1. Compressive creep densifies graphite. This densification occurs during the initial primary creep and is recoverable when the load is removed. Secondary creep occurs at constant volume.

Fig. 6.12. Effect of fluence on the coefficient of thermal expansion showing the effect of applied load.

Fig. 6.13. Effect of creep deformation on the 500°C coefficient of thermal expansion for graphite grades H367 and H451.

2. Compressive creep deformation has virtually no effect on the electrical resistivity, elastic constants, or size of the background flaw distribution. Thus, creep should not affect strength.

3. The 500°C CTE is increased by creep deformation, which implies an increased reduction in the microcracks within the particulate.

6.1.6 Postirradiation Examination of Capsule HTK-4 — C. R. Kennedy

Graphite specimens from HTK-4, irradiated in HFIR at 715°C, have been measured and the results compared to previous 620°C results. The graphite grades irradiated are listed in Table 6.1.

The dimensional changes of representative grades H451, ATR-2E, and ASR-2E are given in Figs. 6.14, 6.15, and 6.16. Figure 6.14 gives the volume changes of ATR-2E for the two lots of material at 715°C in comparison to the 620°C results. As expected, the fluence to maximum densification is reduced with increasing irradiation temperature, and the useful life is similarly reduced. The differences between the two lots

Table 6.1. Graphites irradiated in experiment HTK-4

Graphite grade	Density (g/cm ³)	Electrical resistivity (μΩ-m)		Young's modulus (GPa)		Coefficient of thermal expansion (room temperature to 500°C) (°C ⁻¹)	
		With grain	Against grain	With grain	Against grain	With grain	Against grain
ATR-2E IV	1.81	7.3	7.8	10.0	8.6	4.35 × 10 ⁶	4.62 × 10 ⁶
ATR-2E II	1.72-1.78	8.0	10.1	9.0	8.5	4.50	4.85
ASR-2E III	1.79	9.7	10.3	10.8	8.3	3.86	4.48
H451 (lot 408)	1.74	7.2	7.5	11.1	9.5	3.52	4.20
AXF-5Q	1.78	6.0	7.1	13.8	13.8	7.30	7.30
GraphNOL N3M	1.78	6.0	7.1	10.8	9.1	3.68	4.28
ASR-1R	1.81	9.9	10.4	10.3	8.9	4.57	4.90
ASR-1 R(P)	1.81	9.2	10.0	11.2	10.7	3.49	3.90

Fig. 6.14. Volume changes under irradiation for lots II and IV of graphite grade ATR-2E.

Fig. 6.15. Dimensional changes under irradiation at 715°C for graphite grades H451 and H429.

Fig. 6.16. Comparison of volume changes in HFR, Petten, and High Flux Isotope Reactor for graphite grades ASR-2E, lots II and III.

are very pronounced at 715°C where the ATR-2E II demonstrates an expansion and a significant reduction in density. This is not as pronounced at 620°C; however, the data are limited to essentially the fluence of maximum densification. The differences in expected life of these two presumably similar lots are quite large and should be a concern.

Figure 6.15 presents the results of the dimensional changes of grade H451 (lot 408). These results are compared with past results obtained from irradiating grade H429, a preproduction grade of H451. Also shown in the figure are the limited fluence results from ORR capsule irradiations OG-1, -2, and -3. Obviously all the results are very consistent with one another.

Figure 6.16 compares the volume changes in HFIR with those obtained from irradiation in HFR, Petten, at 750°C for grade ASR-2E. The results are in excellent agreement, considering the slight difference in the irradiation temperature. This finding also gives additional credibility to the absence of a flux effect over this temperature range. In general, these results demonstrate a satisfying agreement between the HFIR irradiations and those performed in the ORR and the HFR.

Young's modulus values calculated from longitudinal and shear sonic velocities were also made. Again, the representative grades H451, ATR-2E, and ASR-2E yielded the results shown in Figs. 6.17, 6.18, and 6.19. Figure 6.17 compares the Young's modulus and the shear modulus values from the 715°C experiment with the 620 and 575°C Young's modulus values. The effect of irradiation temperature is as expected. Again, the two lots of ATR-2E demonstrate significant differences in response to irradiation. Lot II does not demonstrate the same increase in modulus as does lot IV and, in agreement with the dimensional change data, is beginning to demonstrate the fall in modulus that would indicate a reduced life expectancy.

Shown in Figs. 6.18 and 6.19 are the results for grades ASR-2E and H451. The significant decrease in the modulus values for ASR-2E in Fig. 6.18 emphasizes the loss of mechanical integrity in the structure resulting from the volume expansion. However, the modulus has decreased only to original values, whereas the density has decreased to less than

Fig. 6.17. Effect of irradiation on the elastic constants of graphite grade ATR-2E.

Fig. 6.18. Effect of irradiation on the elastic constants of grade ASR-2E.

Fig. 6.19. Effect of irradiation on elastic constants of graphite grade H451, lot 408.

the original unirradiated values. This result illustrates the slightly conservative definition of life by the return to the original density of most conventional grades. The results of evaluating grade H451 are compared in Fig. 6.19 with the OG-series values. As observed, the limited OG results are in good agreement with the HFIR results. In contrast to the results from ATR-2E and ASR-2E, there is no indication that the maximum increase in modulus has been achieved at these fluences.

The loss of electrical conductivity or increase in resistivity is illustrated in Figs. 6.20, 6.21, and 6.22 for grades ATR-2E, ASR-2E, and H451. Although these data are not directly applicable to the design of HTGRs, they do afford an additional measure of the structural quality of the graphite. Figure 6.20 gives the current results for ATR-2E in comparison with the earlier 620°C results. Again, the differences between lots II and IV are quite evident. Lot II material is obviously beginning to degrade, as illustrated by the rapid increase in electrical resistivity.

Fig. 6.20. Effect of irradiation on electrical resistivity for graphite grade ATR-2E.

Fig. 6.21. Effect of irradiation on electrical resistivity for graphite grade ASR-2E.

Fig. 6.22. Effect of irradiation on electrical resistivity for graphite grades H451 and N3M.

The lower loss in electrical resistivity with increasing temperature is in agreement with general expectations, although irradiation at 715°C should yield a shorter life, and the resistivity of the graphite should begin to have a more rapid increase.

Results of measuring the CTE of irradiated specimens are given in Figs. 6.23 and 6.24 for the representative grades ASR-2E, ATR-2E, and H451. In all cases where the 715°C results can be compared to those at 620°C, results demonstrated a more rapid loss of CTE at the higher temperatures. This result is clearly shown in Fig. 6.25, where the effect of irradiation on the CTE is given as a function of the original volumetric CTE (the volumetric CTE is used to eliminate the effect of anisotropy). At both temperatures, the graphites with higher CTEs are reduced more by irradiation.

These results support the concept that a flux effect either is very small or does not exist because comparisons of the ASR-2E results and the

Fig. 6.23. Effect of irradiation on the coefficient of thermal expansion for graphite grade ATR-2E. (a) Lot IV (block 343/77). (b) Lot II (block K216/17).

ORNL-DWG 82-7760

ORNL-DWG 82-7761

Fig. 6.24. Effect of irradiation on the coefficient of thermal expansion. (a) Grade ASR-2E. (b) Grade H451.

Fig. 6.25. Effect of initial coefficient of thermal expansion (CTE) on the CTE change by irradiation to 2×10^{26} neutrons/m² ($E > 50$ keV).

H451 results with HFR, Petten, and ORR demonstrate a very good agreement when the same lots of material are used. Thus, the use of HFIR results should be directly applicable for design considerations of HTGRs.

However, significant differences do seem to occur between lots, particularly between lots II and IV for grade ATR-2E. This situation implies that, unless the process variables responsible for the reduced life can be identified, each lot of material would have to be qualified by irradiation testing before it could be used — an obviously untenable situation. The data in Table 6.1 show that the density of lot II is lower and more variable but, of more significance, that the CTEs vary. This result implies not only that the filler coke in lot II was different in character but also that the packing efficiency or particle size distribution was probably different. These two graphites should be carefully compared. On the other hand, the comparison of grade H451 to preproduction grade H429 does give credibility of the reproducibility of this product from preproduction to full production.

The dimensional and the CTE changes resulting from irradiation are all consistent with the concept of anisotropic crystallographic growth filling the layer plane voids, which normally accommodate the crystallite thermal expansion. The anisotropic growth can result from thermal expansion when the c -axis is about $22 \times 10^{-6} \text{ }^{\circ}\text{C}^{-1}$ and the a -axis expansion is essentially zero. Of course, under irradiation the significant filling of layer plane voids results from displacement damage that causes an overall c -axis growth and an a -axis shrinkage. Thus, the void volume is reduced both by increasing temperature and by irradiation; therefore, as the irradiation temperature increases, there is an initial reduction in void volume thermally, which reduces the volume available to accommodate displacement damage growth. The magnitude of the layer plane void volume affecting the CTE is nominally bound within the particulate and is reasonably independent of density. This situation is the justification for the relationship given in Fig. 6.25. The materials with higher CTEs have a lower void volume than do graphites with lower CTEs. Thus, the lower CTE material has a larger void volume to accommodate the anisotropic growth of irradiation and will require a higher fluence to fill this void volume before internal shearing begins to destroy the continuity of structure. The additional loss of void volume by raising the irradiation temperature from 620 to 715°C causes an accelerated loss in CTE, as shown in Fig. 6.25.

The implication that internal shearing and loss of structural continuity starts very early in the irradiation is supported by increased surface area measurements with slight irradiation exposure. This initial shearing must produce fairly small structural defects compared to the large critical defects that control fracture because the strength is significantly increased. In fact, sonic attenuation measurements show that the large critical defects are actually reduced by the anisotropic growth, particularly from the a -axis shrinkage. Nevertheless, the internal shearing continues to progress until both the void volume available is consumed and the new defect structure is larger than the initial critical defect. The strength of the graphite begins to deteriorate, which generally requires a significant exposure. However, for grade AXF-5Q,

the CTE is very high, indicating a small void volume for accommodation, and the critical defect size is very small. Thus, the internal shearing creates a defect larger than the initial critical defect almost immediately. This graphite does not yield an increase but an initial decrease in strength with irradiation, as seen in Fig. 6.26. Sonic attenuation measurements have confirmed the larger defect size.

The life expectancy at 715°C based on volume expansion to the original volume or density is compared with that at 620°C in Table 6.2. The irradiation of HTK-4 was only 2.5×10^{22} , and the expectancy values beyond this fluence are extrapolations. Of course, the confidence in these values decreases with the greater extrapolations. The higher irradiation temperature reduces the life expectancy, in agreement with past studies. It is interesting to note that both the later grades of ASR-1R and ATR-2E have increased life expectancies similar to H451. It is also worth noting the significantly increased life of GraphNOL N3M, an ORNL developed graphite.

Fig. 6.26. Effect of irradiation on the strength of POCO-AXF graphite at 620°C.

Table 6.2. Life expectancy of graphites irradiated in HTR-4

Grade	Life expectancy (fluence) (neutrons/cm ² × 10 ⁻²²)	
	620°C	715°C
ATR-2E IV	3.4	3.0
ATR-2E II	2.8	2.3
ASR-2E III		2.3
ASR-1R	2.8	2.3
ASR-1R (P)	3.3	2.8
H451		3.0
AXF-5Q		3.1
GraphNOL N3M		3.7

6.2 OXIDATION OF GRAPHITES

6.2.1 Corrosion Experiments — C. D. Bopp

Experiments have continued throughout the year to produce samples for mechanical property measurements (see Sect. 6.2.2). A coherent group of experiments has been conducted on the German candidate reflector graphite ASR-1G, specifically on rods 12.7 mm in diameter at 900 and 1000°C. The corroding atmosphere consisted of helium as a carrier gas at 100 kPa (1 atm) nominal pressure with steam at $7000 \pm 10\%$ parts per million by volume (ppmV). In a steady state the loop gas concentrations ran: hydrogen, $18,000 \pm 20\%$ ppmV; carbon monoxide $16,000 \pm 10\%$ ppmV; and carbon dioxide $400 \pm 20\%$ ppmV. Duration of the experiments was from 10 to 28 d. The corrosion rates, averaged over the total duration of a given experimental run by integrating the carbon monoxide evolution, agreed within experimental error with the total weight loss from the graphite rod. Corrosion rates remained constant, as indicated by carbon monoxide generation, after about the first day. Mean corrosion rates were 5.7×10^{-8} g/(cm²·s) ($\pm 5\%$) at 1000°C and 4.3×10^{-8} g/(cm²·s) ($\pm 15\%$) at 900°C. This weak temperature dependence compared with H451 and thermally purified 2020 graphite apparently results from impurity-catalyzed oxidation at 900°C, as is also apparent from the pitted appearance of the graphite rods after exposure.

In preparation for experiments on non-steady-state oxidation, several experiments were conducted in the loop to check the effect of hydrogen concentration on corrosion. Not only can hydrogen enhance oxidation, presumably by activating iron oxide as a catalyst, but it can also retard oxidation by occupying the active surface sites involved in the intrinsic oxidation. The experiments were conducted at 800°C on H451, in one case at 1 vol % steam and 0.05 vol % H; in the other, at the same steam concentration but at 9 vol % H. The initial oxidation rate was an order of magnitude greater in the second experiment (9% H) but, after 400 h, had decreased to a lesser value. This limiting steady-state value at 400 h agreed with that predicted by the Langmuir-Hinshelwood equation as fitted to H451 graphite at General Atomic.⁷ We thus verify in our experiment the predicted inhibition of corrosion in H451 by active-site "poisoning." More detailed studies on the effect of transient exposure to ordinary air or inert environments are now being planned.

6.2.2 Mechanical Properties of Oxidized Stackpole 2020 Graphite — C. R. Kennedy

Brittle-ring specimens of thermally purified 2020 graphite oxidized at 1000°C have been tested. The specimens were removed from the test loop after one, two, and three weeks, yielding weight losses up to 20%. Radiographs of the specimens confirmed the elimination of the catalytic oxidation, that is, the absence of large cavities typical of oxidized as-received material. However, the radiographs also revealed density gradients of an extraneous nature that may be expected to affect the strength of brittle and C-ring specimens. As the main gas flow went down the center of the stacked rings, we expected to see the maximum loss of density on the inside diameter. However, the two- and three-week specimens also had on the outside diameter low-density regions corresponding to the three slots in the outer quartz protection tube. These regions were not evident in past oxidations, probably because of the small weight losses or because the oxidation was uniform and density gradients would not develop. The influence of these density artifacts on the mechanical properties requires investigation to validate the strength measurements. We performed this investigation as follows:

The location of these density irregularities on the outside diameter can easily be determined by peaking eddy-current response. This allows the option of loading the rings with the lower or higher density regions located in the high-stress volume of the specimen. One-half the specimens were tested with the high density regions located at the high stress regions. The second half of the specimens were oriented to place the high-stress regions at the low-density areas. The results of testing each set of rings did not yield an indication of a strength difference due to specimen orientation, as shown in Figs. 6.27 and 6.28. These figures also show comparisons of the current test results with those obtained from oxidation testing of as-received 2020 graphite. Although there is a trend of lower strength for the purified 2020, it is doubtful that the difference is statistically significant. This trend is also in contrast to the improved strength obtained from testing purified 2020 graphite in compression.

Fig. 6.27. Effect of steam oxidation at 1000°C on the brittle-ring strength of 2800°C heat-treated 2020 graphite, 25.4-mm-diam specimens.

Fig. 6.28. Effect of steam oxidation at 1000°C on the C-ring strength of 2800°C heat-treated 2020 graphite, 25.4-mm-diam specimens.

The effects of oxidation on the statistical characteristics of fracture as it affects the reduction in strength of different size specimens has been examined further. This is relevant in that, if the statistical characteristics of fracture are altered significantly by oxidation, the use of subsize specimens may well underestimate the real loss of strength in full-size blocks. This effect was examined by evaluating the results of testing 19-, 25.4-, and 38-mm-diam brittle-ring and C-ring specimens of 2020 graphite. The results are shown in Fig. 6.29, where the fracture strength is given as a function of stress volume for 17.1, 20, 25, and 30% porosity. The 17.1% porosity line represents the unoxidized as-received material, with the higher porosity curves resulting from oxidation. The significant result is that the slope of each curve is the same. This strongly suggests that the Weibull statistical characteristics of fracture are unaltered. Therefore, the results of subsize testing can be validly related to full-size blocks.

Fig. 6.29. Effect of oxidation on the strength of 2020 graphite. The strength is reduced equally for all specimen sizes tested.

6.2.3 Effect of Oxidation on the Strength of ASR-1RG — C. R. Kennedy

Initial results of brittle-ring testing of grade ASR-1RG graphite indicated a very severe loss in strength due to oxidation in steam. However, we suspected that the gross size of the defect structure in ASR-1RG in contrast to the specimen section thickness, caused the increased strength loss. Therefore, 13-mm rods of ASR-1RG were oxidized at 900 and 1000°C and compression tested to evaluate the potential section size effect. The rods of ASR-1RG graphite, oxidized at 1000°C to approximately 5, 10, and 15 wt % loss, yielded a structural morphology like that shown by previous oxidations of impure rings, with the samples exhibiting large wormholes associated with catalytic oxidation around particles with a high atomic number. The oxidation also had a strong outside-to-inside gradient in mass loss, as seen by radiography of 6.35-mm-thick sections of the rods. This oxidation profile is also seen by comparing the conductivity ratio of surface to center as a function of porosity in Fig. 6.30. The rods oxidized at 900°C were similar to the 1000°C graphite but with a

Fig. 6.30. Ratio of surface-to-end electrical conductivities for 1000°C steam-oxidized ASR-1RG graphite. Loss of conductivity reflects oxidation, specifically an oxidation gradient from exterior (surface) to interior (ends).

lower oxidation density gradient. The overall appearance is very similar to that of the as-received 2020 graphite before thermal purification except for the coarseness of the structure.

The compression test results are compared in Fig. 6.31 with 2020, H451, and previous brittle-ring tests of ASR-1RG. The previous brittle-ring results, as shown in the figure, yielded indications of an extreme loss in strength due to oxidation. The coarseness of the ASR-1RG structure compared with the small brittle-ring specimen casts some doubt about the validity of the results. The compression test results are consistent with 2020 and H451 results and cast doubt on the usefulness of the brittle-ring specimens in evaluating materials with very coarse defect structures.

Fig. 6.31. Comparison of the effect of steam oxidation on the compression strength of ASR-1RG to grades 2020 and H451 graphite and the brittle-ring and C-ring strengths.

6.2.4 Modeling of Steam Penetration into Graphite — C. D. Bopp

A modification⁸ of Thiele's model⁹ may be a suitable approximation, with the limitation that the reaction rate is high enough that the steam is consumed in a zone near the surface. In this case, in first approximation, the surface moves inward at a constant rate as the graphite is consumed. With both Thiele's model and the current modification, in the case of a semi-infinite geometry, the steam gradient is given by

$$\partial \ln(y/y_0)/\partial x = -\sqrt{K/D}, \quad (1)$$

where y is the steam concentration in the graphite pores at a depth x from the surface of the sample, y_0 is the concentration at the surface, and K

is a first-order rate constant. In Thiele's model the quantity D is the effective diffusivity; however, in our case the effective diffusivity depends on the depth. Therefore, D is a parameter without a simple physical meaning, which is obtained from an experimental burn-off profile. (For a first-order reaction the burn-off profile is proportional to the steam gradient.) This approach is suggested by the agreement of burn-off profiles for 2020 graphite at 1000°C with Eq. (1) (ref. 10).

An alternate approach to modeling is to solve the equation for diffusion with chemical reaction and with variable diffusivity.⁹ The limitations of the above approximation are being studied accordingly.

6.2.5 Baseline Microstructural Properties of H451 and 2020 Graphites — D. A. Lee

Graphite samples from reactor-grade core graphite specified for the High-Temperature Gas-Cooled Reactor (HTGR) have been analyzed to characterize their microstructural properties. These analyses have included BET surface area by nitrogen adsorption at 77 K, helium pycnometry, and mercury porosimetry. The surface area and helium pycnometry apparatus has been described.¹¹ Modifications have been made on this apparatus to ensure better dependability of the operation. These modifications include a smaller vacuum-tight manifold, temperature regulation of the manifold and helium pycnometer sample cell to $\pm 0.1^\circ\text{C}$, additional software to give better statistical treatment of the data, and plotting techniques. Improved sample cells have been made both for surface area and helium density analyses. Baffling around the liquid nitrogen probe and stirring the liquid nitrogen have improved precision for surface area analyses. A separate sample degassing manifold has been built; use of this manifold reduced sample analysis time threefold.

Mercury porosimetry determinations were made by standard methods on a modified Aminco porosimeter. A rotating stand for the initial specimen evacuation and mercury penetration permits measurements at subatmospheric pressures (from a pore hydraulic diameter of 100 μm on down). Pressure measurements are effected on 30-cm-diam scaled Bourdon gages with a claimed full-scale accuracy of 0.1%. Computer programs have been written for the acquisition of mercury porosimetry data. The data can be printed on hard copy, and two types of porosimetry plots can be plotted with a Hewlett Packard 9827A plotter.

The ASTM round robin study of the measurement of reactor-grade graphite samples for surface areas and helium densities has been completed. A statistical analysis is given in Table 6.3. The data subsets are internally consistent, and the "precision" is excellent.

Table 6.3. Statistical characteristics of surface areas and densities

Graphite type	Specimen group	Surface area (m^2/g)			Helium density (Mg/m^3)		
		Value	Standard deviation		Value	Standard deviation	
			Of mean	Of sample distribution		Of mean	Of sample distribution
H451	5-g	0.928	0.072	0.161	2.054	0.004	0.009
	0.5-g	0.994	0.089	0.198	2.066	0.019	0.042
	All	0.961	0.055	0.173	2.060	0.009	0.029
2020	5-g	0.394	0.025	0.057	2.022	0.007	0.016
	0.5-g	0.334	0.029	0.065	2.026	0.002	0.005
	All	0.364	0.021	0.066	2.024	0.004	0.012

6.2.6 Corrosion-Induced Microstructural Changes in H451 Graphite — D. A. Lee and W. P. Eatherly

A number of oxidized H451 graphite specimens have been examined for changes in surface areas, helium densities, and mercury porosimetry as a function of weight loss. The samples so examined were selected from among those to be tested for mechanical properties and may therefore be considered from the same populations. Burn-offs were calculated for individual specimens from both the final density of each specimen and the original unoxidized density of the rod from which specimens were abstracted.

Currently available results are given in Figs. 6.32 through 6.34. All values are given on a specific volume basis, that is, that for a cubic centimeter of "average specimen": oxidation gradients existed in one form or another in all specimens, and the parameters apply to the specimen average value and specimen average burn-off.

Because the oxidation apparatus operated in a steady-state mode, hydrogen-to-water ratios varied considerably from run to run as the water input and removal rates varied. The range in this ratio ran from a high

Fig. 6.32. Total BET nitrogen surface areas. Open and half-shaded points indicate inside-out oxidation profiles.

Fig. 6.33. Accessible void volume based on helium and bulk densities. Open and half-shaded points indicate inside-out oxidation profiles.

Fig. 6.34. Inaccessible void volume based on helium density. A theoretical density of 2.270 g/cm^3 is assumed. Open and half-shaded points indicate inside-out oxidation profiles.

of 20 to a low of 0.5. Assuming iron to be the major catalytic agent, it could presumably become active only in the sample interior when the gas stream ratio approached 3. Such "inside-out" oxidized specimens are noted on the figures as partially filled or unfilled points. Such points cannot be expected to lie on the same curve as the normally oxidized material.

We observed a very rapid increase in total BET surface area and accessible void volume at the expense of inaccessible volume for total burn-offs under about 2%. The effects are less pronounced than in A3-3 for the reasons mentioned above but are still quite dramatic. This effect is undoubtedly due to a rapid initial attack by the steam on the less graphitic binder carbon. A continuing rather linear attack extends even up to greater than 10% burn-off. These trends hold whether expressed for specific volume or specific mass. These data must yet be corrected for burn-off profile.

6.2.7 Corrosion-Induced Changes in German A3-Type Fuel Matrix Material — P. Krautwasser* and W. P. Eatherly

In collaboration with personnel of the Hahn-Meitner Institute, studies of several varieties of fuel matrix graphite were undertaken in support of their fission product migration program. Samples included lightly oxidized material of some interest in the general case of graphite corrosion, because these samples cover the initial rapid change in surface area from oxidation. The materials studied were

- A3-3 (1800°C), a material consisting of natural graphite and electrographite with a binder of phenolformaldehyde resin. The final heat treatment of the material was at 1800°C.
- A3-3 (1800°C) with different degrees of CO₂ oxidation (corrosion), ranging from 0 to 5 wt % loss.
- A3-3 (1950°C), an A3-3 material with a final heat treatment at 1950°C to increase strength and decrease reactivity.
- A3-27 (1950°C), an alternate material to A3-3, consisting of natural graphite, electrographite, and a binder based on in-place polymerization of phenol and hexamethylene tetramine. The final heat treatment was 1950°C.

In addition to the surface areas, helium densities, and mercury porosimetry, small-angle x-ray scattering (SAXS) was used to examine pore changes in the nanometer range.

6.2.7.1 Oxidation of A3-3 (1800°C) Matrix Material

An increase in weight loss during oxidation of the material leads to an increase of pores ranging in diameter from 1.5 to 20 nm (SAXS) and from 0.4 to 1.5 μm (mercury porosimetry) (Table 6.4 and Fig. 6.35). This increase is accompanied by an increase in accessible pore volume as determined by the apparent material density in helium as well as by a pronounced increase in total BET surface area. Conversely, as might be expected, the inaccessible pore volume decreases.

*On assignment to ORNL from KFA Jülich, West Germany.

Table 6.4. Data on oxidized A3-3 (1800°C) material^a

Sample	Burn-off (%)	Bulk density (g/cm ³)		Pore volume (%)				Surface area (m ² /cm ³)
		Original	Oxidized	>1 μm	>0.4 μm	Accessible	Inaccessible	
1	0	1.700	1.700	11.62	14.79	15.80	9.32	0.236
5	0.1	1.699	1.697	11.15	14.49	16.53	8.72	0.437
7	0.5	1.694	1.686	9.85	13.12	20.21	5.53	1.70
4	1.0	1.697	1.680	10.00	13.68	19.69	6.30	7.19
6	5.0	1.745	1.658	9.58	14.34	22.49	4.48	11.2
8	3.0	1.664	1.614			23.54	5.36	13.3

^aOriginal densities were calculated from oxidized densities and quoted burn-offs. Inaccessible pore volume assumes a theoretical density of 2.27 g/cm³.

Fig. 6.35. Size distribution of pores ranging from 0.4 to 4.0 μm in diameter in A3-3 oxidized material.

The sample with the smallest weight loss (0.1 wt %) shows a decrease in concentration of the very small pores with $d = 1.0 \mu m$. This decrease indicates that, at the beginning of oxidation, the existing fine pores are enlarged. In addition, further oxidation leads to the development of micropores smaller than 2.0 μm .

Some data of the samples with 3 and 5 wt % loss are inconsistent with other results. A recalculation of the initial densities from the densities of the oxidized samples and the given weight loss yield a low initial density for the 3% sample and a high value for the sample with 5 wt % loss. The differences in other material properties may therefore be in part or entirely due to differences in the initial sample microstructure.

6.2.7.2 A3-3 Material Heat Treated at 1950°C

Compared to the A3-3 material heat treated at 1800°C, the increase in heat treatment temperature to 1950°C leads to a decrease of pores in the

diameter range of 10 to 20 nm (SAXS) and in the range of 1.0 to 2.5 μm (Table 6.5). In this wide range, the peak of the pore size distribution shifts with increasing temperature from $d = 1.8 \mu\text{m}$ to $d = 0.9 \mu\text{m}$ (Fig. 6.36). This shift and the decrease in larger pores lead to a lower BET surface area, accessible pore volume, and apparent density in helium. An increase in inaccessible pore volume is caused by a shift of the pore size distribution to smaller pores and therefore an increase in small micropores with $d \leq 5 \text{ nm}$, as seen by SAXS. The observed reduction of lattice c spacing with heat treatment, especially in the binder, leads to an increase in bulk density.

Fig. 6.36. Size distribution of pores ranging from 0.4 to 4.0 μm in diameter in A3-3 (1800°C), A3-3 (1950°C), and A3-27 (1950°C) oxidized materials.

6.2.7.3 A3-27 Material Heat Treated at 1950°C

Comparison of A3-27 (1950°C) with A3-3 (1950°C) shows that the difference in binder polymerization leads to a higher bulk density, a

Table 6.5. Characterization of three fuel matrix types^a

Material type	Bulk density (g/cm ³)	Pore volume (%)				Surface area (m ² /cm ³)
		>1 μm	>0.4 μm	Accessible	Inaccessible	
A3-3 (1800°C)	1.706 ± 0.009 (2)	11.62	14.79	14.56 ± 1.76 (2)	10.82 ± 1.3 (2)	0.224 ± 0.016 (2)
(1950°C)	1.724 ± 0.031 (4)	5.39	9.71	11.14 ± 1.38 (4)	12.93 ± 0.74 (4)	0.181 ± 0.006 (2)
A3-27 (1950°C)	1.766 ± 0.002 (2)	5.82	7.04	9.58 ± 0.18 (2)	12.65 ± 0.27 (2)	0.432 (1)

^aWhere applicable, values are given as means with population standard deviations and, in parentheses, number of samples.

somewhat lower accessible pore volume, and a decrease in pores with $d > 0.4 \mu\text{m}$ (Table 6.5). The maximum in pore size distributions of A3-27 material is at $d = 1.25 \mu\text{m}$, between the peaks of the A3-3 (1800°C) and A3-3 (1950°C) materials. The inaccessible pore volume in A3-27 (1950°C) is about the same as in A3-3 (1950°C).

Compared to A3-3 (1800°C), the increase in final heat treatment leads, as well as in A3-3 (1950°C), to an increase in pores ranging from 1.0 to 5.0 nm and a decrease in 5.0- to 20.0-nm pores.

The oxidation series of A3-3 (1800°C) samples shows the anticipated effects of an immediate increase in total surface area (BET) and accessible void volume at the very lightest burn-offs. This is consistent with graphite behavior generally under steam or carbon dioxide oxidation but is accentuated here, presumably because of the nongraphitizable binder and low heat treatment temperature. The pore textures coarsen, although the effects are more marked in the micropore region ($<20\text{-nm}$ diam) than in the macropore region ($>0.4\text{-}\mu\text{m}$ diam).

Both A3-3 (1950°C) and A3-27 (1950°C) show a normal behavior to increased heat treatment temperature with increased densification and inaccessible pore volume and decreased total surface area (BET) and accessible pore volume. These effects are attributable to the resin binder shrinkage as the last hydrogen is eliminated and incipient crystallization occurs. The lack of well-developed long-range order leads concurrently to an increase in the micropore volume ($<5\text{-nm}$ diam).

6.3 STANDARDIZATION OF GRAPHITE — STATISTICAL IDENTIFICATION OF DISPARATE FLAWS IN H451 GRAPHITE — W. P. Eatherly

Flaws in bulk graphite determine the tensile or flexural strength and may be broadly classed as either background or disparate. The background flaws are inherent in the material and are generally comparable to the grain size, whereas the disparate flaws are introduced during forming and heat treatment and can be quite large. In aerospace graphites with their small billet and grain size, sonic attenuation characterizes the omnipresent background flaws, and sonic reflection readily detects disparate flaws.¹²

For nuclear and most structural graphites, the large billet and grain size generally preclude sonic techniques for flaw identification and location. This study assumed that nuclear graphites would possess the same flaw characteristics as aerospace graphites, a bimodal distribution of strengths representing a normal distribution with a low-strength tail attributable to the disparate flaws. This is hardly an assumption, as even a superficial look at the microstructure will substantiate. The statistical problem then becomes the identification and characterization of these two components of the total distribution of strengths. Our purpose is not to develop acceptance-rejection criteria, although such are readily obtainable from our results.

Data available for study¹³ consisted of three production runs of Great Lakes graphite grade H451, representing 14, 56, and 28 billets. From the center of each billet, sets of tensile samples were obtained in both the radial and axial directions, with cylindrical stress volumes defined by diameters of 6.35 mm and lengths of 23 mm. The data analyzed here are for the axial strengths only and consist of a nominal sampling of eight specimens for each billet.

This analysis is preliminary only, and we push it through to its logical end without attempting at this point to prove and to justify the individual hypotheses and tests. A much more detailed analysis has been initiated, including the complete data set, and this guards against any unwarranted assumptions or statistical correlation, which would impair these assumptions. Obviously such an analysis is much more delicate and elaborate and is hardly worth the effort unless this preliminary analysis is successful.

6.3.1 Plan of Attack

In attacking problems of multiple items with inherent variability, it is customary to hypothesize that they can be characterized by two variances, one within and one between items. In our case, we take the variance σ_w^2 to represent the within-billet variability and the variance σ_b^2 to represent the variability from billet to billet during the production run. These hypotheses are sufficient to define the variance problem termed the "one-way layout with random effects," for which standard methods of solution are available.¹⁴

From the set of eight specimens of each billet, we may obtain an observed estimate s_i^2 of the within-billet variance, i denoting the billet number. Presumably, the s_i^2 all are estimates of σ_w^2 . If true, the set of sample variances $\{s_i^2\}$ is termed homogeneous, and the one-way variance is validated. Our hypothesis that the background flaws are random and omnipresent is sufficient to guarantee the homogeneity of the variances s_i^2 . Conversely, lack of homogeneity can be taken to imply the expected existence of the bimodal distribution arising from the addition of disparate flaws. We shall find the Bartlett χ^2 -test for homogeneity¹⁵ to be a sufficient test.

Having presumably found lack of homogeneity, we must now proceed to identify those measurements that do not belong to the normal distribution and "reject" them as being caused by disparate flaws. The procedures for eliminating such outlier values are manifold. The types that we wish to use should utilize our best estimate s_w^2 rather than the individual s_i^2 . For our purposes, the Nair one-sided test¹⁵ and the Studentized Range Test¹⁶ are sufficient. The data sets remaining after these data rejections become the "truncated data."

The truncated data are resubjected to the homogeneity test, and the s_i^2 are now homogeneous. The variance analysis can then be repeated on the truncated data with confidence. The resulting parameters are a valid representation of the graphite "between the cracks." Conversely, the rejected data now characterize the "cracks" and, in this case at least, also turn out to be normally distributed but obviously displaced downward in strength from the background-flaw-induced strengths.

With the distributions now characterized, it is clearly an easy task to determine criteria by which billets are to be rejected to guarantee minimum strength at any desired confidence level.

6.3.2 Mathematic Details

We assume in this section the reader's familiarity with the general statistical principles involved. For those not concerned, the results are summarized in our concluding section below.

The results of the provisional variance analysis for the three lots are given in the first part of Table 6.6. The two statistical parameters characterizing the distributions are s_w^2 and $\hat{\theta}$:

$$E(s_w^2) = \sigma_w^2, \quad E(\hat{\theta}) = \theta = \sigma_b^2 / \sigma_w^2.$$

Table 6.6. One-way variance analyses and homogeneity tests before and after truncation

	Full distributions			Truncated distributions		
	Lot 472	Lot 478	Lot 482	Lot 472	Lot 478	Lot 482
$m, \text{ psi}^a$	1779	1979	1615	1771	2013	1570
$s_w, \text{ psi}^a$	207	247	228	156	213	208
$\hat{\theta}$	3.29	1.62	1.98	5.52	2.30	2.51
v_1	13	55	27	12	51	26
v_2	98	392	196	91	364	189
K	37.5	108.7	37.6	15.0	68.0	28.9
$\chi^2(v_1)$	22.4	73.3	40.1	21.0	68.7	38.9
Q	0.127	0.027	0.049	0.099	0.025	0.050
$q(v_1, v_2)$	0.103	0.024	0.049	0.112	0.027	0.050

$a_{\text{psi}} = 6.89 \text{ kPa.}$

The degrees of freedom for the mean sum of squares for σ_w^2 is v_1 and for the remainder of the partitioned sum (Scheffé's MS_A) is v_2 . Also given are the mean strengths m for the lots.

Two tests for homogeneity were considered, the Bartlett test in its χ^2 form and the Foster Q-test.¹⁵ The latter test was rejected because of its lack of sensitivity to large values of s_L^2 unless these are far off the expected χ^2 -distribution. This flaw appears to be inherent in the Q-test. The criterion for homogeneity in the Bartlett test is that the observed statistic K be less than the upper α th critical point for the χ^2 -distribution, and, in the Foster test, that the observed statistic Q be less than the α th critical point of the q -distribution. The reader is

referred to reference 15 for the forms of K and Q . Clearly, the complete data sets do not satisfy either criterion for lots 472 and 478, and that for lot 482 does so only marginally.

Data "rejection" was accomplished on the basis of Nair and Studentized Range Tests,¹⁶ the one to test for low-lying outliers, and the other for kurtosis. We also used the common restriction for small data sets: if the within-billet data subset required removal of more than one datum, the entire subset was rejected. The choice of risk α for these criteria is somewhat arbitrary, because the underlying distribution of flaws must be continuous and the dichotomization into disparate and background flaws is itself arbitrary. The choices $\alpha = 0.05$ for the Nair and 0.10 for the Range test were sufficient to obtain variance homogeneity. The number of "rejected" data points and subsets are given in Table 6.7.

Table 6.7. Rejection of data to achieve homogeneity

Lot	Single datum (outlier)	Entire billet (outliers)	8 Data (range)
472	3	0	1
478	7	3	1
482	3	0	1

The results of the variance analysis and the homogeneity tests are given in the second part of Table 6.6 for the truncated distributions. Partially filled cells (i.e., billets for which less than eight specimens were available) were treated as "filled" by leaving the mean and variance unchanged but increasing the degrees of freedom arbitrarily. By t -test on the means and F -test on $\hat{\theta}$, the three lots are different.

The rejected data, to the extent they contained low outliers, whether isolated or from rejected subsets, were now taken to represent specimens with disparate flaws. For brevity, we consider only the collective results from all three lots. For the specimen volume employed, the

probability of occurrence of a disparate flaw is 3.4% (26 flawed specimens from a total population of 774). Binomial analysis indicated a slight correlation effect, that is, a tendency to find multiple disparates within a billet.

The strengths of the 26 disparate specimens were normally distributed, which was not surprising because they were identified by their distance from the normally distributed billet means. The parameters are given in Table 6.8; the moment correlation coefficient R is that for strength deficiency versus argument of the cumulative distribution function by use of the formula $\phi = (8i - 3)/(8n + 5)$ for the sample cumulative. Clearly the normality is excellent.

Table 6.8. Parameterization of disparates

Distance, mean below billet mean, psi ^a	768
Standard deviation, psi ^a	133
Correlation coefficient	0.9931

$$^a_{\text{psi}} = 6.89 \text{ kPa.}$$

6.3.3 Conclusions

We summarize our results by stating that the distribution of strengths for a production lot may be characterized by three normal distributions. The numerical values for the parameters involved will, of course, be mildly affected by the specimen stress volume. The three distributions are those for

1. the billet mean strength with inherent variability given by the standard deviation (see the second part of Table 6.6):

$$s_b = \sqrt{\hat{\theta}} s_w ;$$

2. the variability within a billet given by the standard deviation s_w (Table 6.6); and

3. an additional distribution due to disparate flaws with the mean lying 5.30 MPa (768 psi) below the billet mean, a standard deviation of 0.92 MPa (133 psi), and occurring for 3.4% of the specimens (Table 6.8). One obviously does not reject high-strength billets because of disparities. Rather, the low-strength billets must be rejected at a higher tolerance level.

Unfortunately, the specimens used were destroyed before this analysis was performed. It is now impossible to verify that the specimens identified did indeed contain large flaws, but any other explanation would be forced.

We conclude that this preliminary analysis appears to justify a much more complete study of the statistical properties of H451 graphite. Clearly the effective strength of the material can be improved by billet truncation, or alternatively, by employing fabrication techniques leading to elimination or reduction of the disparate flaws.

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7. HIGH-TEMPERATURE REACTOR PHYSICS STUDIES

D. E. Bartine

7.1 INTRODUCTION — B. A. Worley

The analysis of high-temperature gas-cooled reactors (HTGRs) requires numerical methods capable of modeling a large thermal reactor core. Application of the models requires expensive calculations coupled through complicated chains of computer codes. In addition, the use of these analysis tools requires understanding of the various details of the models.

Work performed this past year has been directed toward improvement of finite-difference diffusion-theory calculations with emphasis on improved accuracy for some applications and reduced computational time for others. Mesh-edged finite-difference formulations¹ and a fuel management (with economics) module² were completed for use with the local neutronics code VENTURE.³

Analysis of the HTGR was concentrated in those areas that looked promising for a better understanding of the fuel performance but without the expense of full-core, three-dimensional calculations. A three-dimensional model of the fundamental unit cell made up of a partial fuel element from each refueling batch was used to study power density variation within a region containing fuel elements with quite different burnups. Schemes to correlate flux variation and boron cross section with burnup were investigated. A perturbation scheme capable of predicting changes in reactor conditions with fuel depletion shows some promise after preliminary analysis. Coupling of HTGR thermal-hydraulic capability to VENTURE was initiated.

The pebble-bed reactor (PBR) concept has been followed under this task for several years. This past year calculations were performed to evaluate control rod worth for a 1640-MW(t) PBR.

A report covering pebble-bed thermal hydraulics work was published.⁴

7.2 HIGH-TEMPERATURE REACTOR METHODS DEVELOPMENT

7.2.1 Diffusion-Theory Neutronics — D. R. Vondy

The new VALE¹ neutronics code was tested for its use as a three-dimensional, triangular-Z, mesh-edged finite-difference diffusion theory code for high-temperature reactor (HTR) application. This code was originally developed for fast reactor application under separate base physics method funding. Both VALE and the mesh-centered finite-difference code VENTURE are used with an exposure code and a fuel management code to follow reactor fuel cycle histories.

Some difficulties were found in generating neutronics solutions to importance problems. The general procedure in the VENTURE code is to remove any contamination in the source for each outer iteration by calculating its contribution from the previously generated regular and adjoint solutions to the fundamental problem and the orthogonality of the fundamental and importance solutions. However, only a limited set of all possible sources is associated with an orthogonal solution, and the solution process produces a pseudoconverged state if the importance source is not chosen correctly. We modified the VENTURE code to allow extended testing, to display additional data so that the situation can be analyzed, and to print a warning to the user when only a pseudoconverged state is calculated. (The burden of supplying an acceptable importance source that contains positive and negative components lies with other procedures external to the neutronics code.)

7.2.2 Fuel Management and Economics Module — D. R. Vondy and T. Medlin*

The capability for automated fuel movement on refueling, for accounting of isotope inventories in and out of core, and for fuel cycle economic calculations was coded into a fuel management module² that interfaces with the VENTURE neutronic code system.

An extension of the module was found to be necessary when case histories were followed. For example, an idealized repeating cycle could be described by assuming a simple repeating specification for quarter-core refueling of discharging zone 4; for moving material from zones 3 to 4,

*Technology for Energy Corp.

2 to 3, and 1 to 2; and then for refueling zone 1. This specification would be applied to each refueling operation. The movement of fuel would actually not be required but would be specified for accounting purposes. An obvious limitation of such an idealized scheme is that the zones would have to be of equal size along the fictitious path of movement; otherwise there would be a discrepancy. To provide more general and flexible capability, the fuel management code was extended to allow the alternative procedure of cycling through a list of refueling specifications. Thus, zones 1, 2, 3, and 4 could be discharged and refueled at successive refueling steps, and this list could be cycled throughout the history so that the original modeling limitations could be avoided.

The procedures for calculating fuel cycle costs given the actinide mass balances from the fuel management module were implemented. The wide variety of problem types and associated details of the fueling history impacts the procedures of calculation; therefore, only a limited set of costing schemes, hopefully the most useful, was treated.

7.2.3 Reflector Response Methods — D. R. Vondy and B. A. Worley

The use of boundary conditions at the core-reflector interface would substantially decrease the number of mesh points in the finite-difference solution of core neutronics problems. We started to determine the accuracy of group-to-group response matrix boundary conditions as compared with full core-plus-reflector treatment, to assess the feasibility of implementing the use of such data, and to establish the data requirements.

7.3 HIGH-TEMPERATURE GAS-COOLED REACTOR ANALYSIS

7.3.1 A Three-Dimensional Model for HTGR Analysis — D. R. Vondy

There is considerable interest in the effect on core performance of variations in the key design parameters. Full three-dimensional core calculations for parametric analyses are prohibitively expensive with the methods available locally. The quasi-equilibrium state that establishes after some period of operation with a fixed fuel management scheme is of special interest for parametric studies of reactor histories. Finding the required fissile feed enrichment that results in a desired end-of-cycle reactivity is a difficult problem. It is further complicated by the need

to effect low fuel temperatures by spatial variation in the fuel enrichment, by selective use of burnable poison, and by preferred control rod positioning. In addition, an effective burnable poison loading scheme to decrease the initial core reactivity and to compensate for the decrease in reactivity with fuel burnup must also be demonstrated.

The one-dimensional axial model is capable of representing the individual fuel batches with a subzone scheme. However, the horizontal plane is ignored, and no attempt is made in such a calculation to account for variation of the neutron flux across the fuel assemblies. The individual batches of fuel with various ages are assumed to contribute linearly to the macroscopic cross sections at each axial position.

We investigated a more realistic model in which a three-dimensional representation of a cell is modeled through the axial length of the core (lacking only the radial effect of the full three-dimensional problem). The trick is to effect a reasonable approximation to the geometric arrangement of adjacent batches of fuel of various ages. With annual one-quarter core refueling, individual batches would have 0, 1, 2, and 3 years exposure after refueling. A limitation does come from the geometric approximation: a simple cell with a regular arrangement of mesh points and simple boundaries does not truly represent the real situation of abutting hexagonal fuel assemblies. An example of a cell approximating a patch of core with 1/3 core refueling is shown in Fig. 7.1. Such models were found to be useful for parameter studies to avoid the high cost of the full-core three-dimensional core representation.

7.3.2 Redepletion Schemes — D. R. Vondy

A rather serious difficulty became apparent regarding the usual method of calculation, in which separation of space and time is assumed. In applying the successive neutron flux and exposure calculations, we needed to take eight steps between successive annual refuelings, even when a simultaneous solution of the equilibrium ^{135}Xe losses was performed in the neutronics problem. With only six steps over a 292-d exposure period, a quasi-equilibrium result could not be established because a false shift in the flux peak occurred toward the bottom of the core late in the cycle. We would like to perform parameter studies at a lower cost than that for many exposure steps between refuelings.

NOTE: THE CROSS SECTION HATCHING SHOWS
FUEL OF THREE DIFFERENT AGES, 7
HEXAGONAL FUEL ASSEMBLIES PER
PATCH

Fig. 7.1. View of a cross section through the High-Temperature Reactor core fuel assemblies.

The difficulty prompted the testing of a redepletion procedure. Considering that the initial and final neutron flux distributions within the core are estimated, a simple average of these spatial flux distributions can be performed and the exposure calculation done in a single time step. It should then be possible to recalculate initial and final conditions in an iterative process to move rapidly toward the quasi-equilibrium state. However, tests of this procedure to date indicate that the solution process is often not stable in that the flux peak shifts from the top of the core toward the bottom over the exposure period. Nevertheless, the method is so attractive that further study is being carried out to identify some technique for stabilizing the process. Note that with partial core refueling much of the contents of the reactor comes from the effect of prior exposure of material; a quasi-equilibrium solution may be established only after iteration from some assumed initial state to an acceptable degree of convergence.

The tendency of the neutron flux to flip from peaking toward the bottom does raise the question of the possible need to use a higher heavy metal loading (lower rate of carbon to heavy metal) to tighten the neutron coupling and reduce the tendency toward instability.

7.3.3 Boron Cross Section Versus Burnup — D. R. Vondy

Study of the variation in the thermal absorption cross section of ^{10}B in an unfueled fuel hole shows a good representation over the range of interest by using the formulation

$$\sigma_{a,\text{th}} = 0.004933 / (3.179 \times 10^{-6} + N) ,$$

where N is the smeared density of ^{10}B and decreases with exposure. Of interest to us was the form of an accurate correlation.

7.3.4 Depletion-Perturbation Calculations — J. R. White and J. P. Renier

In recent years, ORNL has been heavily involved in the development and application of generalized perturbation theory (GPT) methods to the broad areas of reactor physics and thermal-hydraulics analyses. The DEPTH-CHARGE^{5,6} code system has evolved from the physics portion of the GPT work and represents the formal implementation of time-dependent perturbation theory as applied to realistic fuel depletion problems. The DEPTH-CHARGE system contains both nuclide perturbation and cross-section sensitivity formulations, with the more familiar static GPT formulation contained as a special case of the time-dependent methodology. This newly developed calculation capability represents an attractive alternative analytical and computational tool for certain types of reactor physics and design optimization studies.

Recently, General Atomic Company (GA) has become interested in the application of perturbation theory methods to several aspects of its HTGR design analyses. Although several specific application areas have been identified, GA's most immediate concern is the use of GPT methods as an aid in understanding and solving several problems related to the axial and radial fuel zoning in HTGRs (so as to reduce peak fuel temperatures).

Although there is applications experience with GTP methods, most of this is associated with the analysis of homogeneous and heterogeneous fast reactor systems. Therefore, an HTGR demonstration problem was studied to determine the applicability of depletion-perturbation theory to the analysis of thermal reactors.

The results obtained to date indicate a good degree of success. For example, the results listed in Table 7.1 are the changes in power distribution between the top and bottom of the core for various changes in reactor parameters as predicted by application of the calculated sensitivity coefficients and by direct calculation. The GPT results indicate that a relatively good accuracy can be achieved by the single calculation of sensitivity coefficients over a given reactor fuel cycle as compared with the expensive alternative of recalculating the reactor history for each design change. The promise of core design optimization seems evident.

Table 7.1. Comparison of generalized perturbation theory (GPT) and direct calculation for HTGR demonstration problem

Case ^a	Method	Sensitivity coefficient $(\Delta R/R)/(\Delta N/N)^b$					
		P_{TOP}/P_{TOTAL}		P_{BOT}/P_{TOTAL}		k_{eff}	
		BOC	EOC	BOC	EOC	BOC	EOC
1	Direct	3.8	0.56	-7.1	-0.62	0.23	0.22
	GPT	4.3	0.55	-8.2	-0.36	0.20	0.22
2	Direct	-2.4	-0.27	5.2	0.26	-0.11	-0.13
	GPT	-2.5	-0.35	4.8	0.27	-0.12	-0.14
3	Direct	1.6	-0.14	-4.1	0.22	-0.03	-0.04
	GPT	1.7	-0.10	-4.5	0.22	-0.03	-0.04

^aCase 1 — 5% increase in ^{12}C in top fuel region.

Case 2 — 5% increase in ^{238}U in top fuel region.

Case 3 — 5% increase in ^{232}Th in bottom fuel region.

^b $\Delta R/R$ is the fractional response change as a result of a fractional change in nuclide number density $\Delta N/N$. R can be any desired parameter, such as power fraction in the top of the core (P_{TOP}/P_{TOTAL}) or bottom of the core (P_{BOT}/P_{TOTAL}), k_{eff} , etc. BOC and EOC stand for beginning-of-cycle and end-of-cycle, respectively.

We are testing further the applicability and feasibility of depletion-perturbation theory to HTGR parameter studies.

7.3.5 HTGR Thermal-Hydraulic Analysis — D. R. Vondy

A variety of special models of the core appear to be attractive for addressing specific aspects of the design without full-core modeling. For example, one-dimensional axial traverse calculations produce the results shown in Table 7.2. Note that raising the inlet coolant temperature lowers the peak fuel temperature but increases the axial average peak fuel temperature. A larger decrease is predicted by GA because the largest region peaking factor is usually higher than 1.2.

Local capability to solve the thermal hydraulics problem for the HTR core is needed to fully utilize core analysis studies. Rather complicated requirements for both full and partial core modeling impact methods implementation. One possibility is to adapt the GA thermal hydraulic code POKE, coupling it into the ORNL core physics codes.

Table 7.2 HTGR one-dimensional axial traverse results for coolant outlet temperature of 650°C

Inlet coolant temperature (°C)	Fuel temperature (°C)	
	Axial peak (average)	Peak for 1.2 region peaking factor
350	852.6	973.9
400	853.7	960.9
450	854.6	948.3

7.4 PEBBLE-BED REACTOR ANALYSIS

7.4.1 Control-Rod Worth for a 1640-MW(t) Pebble-Bed Reactor — B. A. Worley and J. S. Tang

Reactivity control and shutdown capability for the PBR are provided by control rods that penetrate an upper graphite reflector and an upper void above the core. If required, the rods are forced into the bed of pebbles.

Diffusion theory is not adequate for treating large voided regions such as the upper void above the PBR core, and available deterministic transport theory codes cannot treat the three-dimensional core geometry with its numerous control rods bounded by a cylindrical core and reflector. Work was performed to demonstrate the application of Monte Carlo and a Monte Carlo Perturbation Source Method (MCPSM)⁷ to this complicated calculation and to present results for a range of problems of interest. The MCPSM method calculates changes in the neutron multiplication factor due to cross-section perturbations, and it is not limited to problems in which first-order perturbation theory is applicable.

The reactor modeled was the 1640-MW(t) reactor for direct cycle application as proposed in Germany. Pertinent data are shown in Table 7.3.

The reactor was first modeled in two dimensional *R-Z* geometry (θ geometry) with diffusion theory and transport theory. Diffusion theory was used to obtain an equilibrium state of the continuously fueled reactor by using the VENTURE code system. Transport theory results for the same geometry (smeared upper void and reflector), and base data were obtained with the DOT-IV discrete-ordinates code.⁸ Once compatibility of data was assured for these calculations, the upper void, upper reflector, and a central control rod were treated explicitly in *R-Z* geometry in DOT-IV to obtain a benchmark against which a three-dimensional representation using Monte Carlo could be compared on an exactly equivalent basis. The Monte-Carlo calculations used the KENO-IV code.⁹ All calculations used six energy groups.

The results of these calculations are shown in Table 7.4. As indicated, the Monte Carlo and discrete-ordinates results compare very favorably.

The reactor was modeled, again in three dimensions, with all the control rods (85 rods, 1 rod/1.57 m²) represented explicitly. Full-core, three-dimensional Monte Carlo calculations were performed, and results of particular interest are presented in Table 7.5. Also shown in Table 7.5 is information concerning the applicability of MCPSM.

The results summarized herein are pertinent to the PBR program because the capability of providing reactivity control in PBRs without

Table 7.3. Data of the reference Pebble-Bed Reactor

<u>Reactor core</u>			
Power, MW(t)	1640		
Power density, MW/m ³	5.511		
Active core height, m	5.50		
Active core radius, m	4.15		
Volume fraction of pebbles	0.61		
<u>Pebble design</u>			
Diameter, cm	6.0		
Graphite shell thickness, cm	0.5		
Graphite density, Mg/m ³	1.70		
<u>Coated particle design</u>			
Kernel material	(U,Th)O ₂		
Kernel diameter, μ m	400		
Kernel density, Mg/m ³	9.50		
Uranium enrichment	0.93		
Graphite coatings, thickness, μ m	85/30/80		
Graphite coating density, Mg/m ³	1.0/1.6/1.85		
<u>Control rod model</u>			
Inner region (helium) outer radius, cm	3.5		
Inner iron outer radius, cm	4.5		
B ₄ C absorber outer radius, cm	5.5		
Outer iron radius, cm	6.5		
<u>Reflectors</u>			
	<u>Top</u>	<u>Bottom</u>	<u>Radial</u>
Thickness, m	1.0	1.0	1.0
Graphite density, Mg/m ³	1.40	1.40	1.60
Upper void, m	1.0		

Table 7.4. Calculated results of R-Z model

Code	Calculation method	Geometry ^a	Central control rod insertion	k_{eff}	CPU ^b (min)
VENTURE	Difussion theory	Smeared	No	0.9911	2.0
DOT-IV	Discrete ordinates	Smeared	No	0.9867	58.0
		Explicit	Bottom of top reflector	0.9790	71.7
		Explicit	Bottom of core	0.9736	70.5
KENO	Monte Carlo	Explicit	Bottom of top reflector	0.9780 ± 0.0018	85.8
		Explicit	Bottom of core	0.0041 ^c ± 0.0037	85.8

^aTop reflector, absorber, and void representation. In the explicit representation, rods other than the central rod are represented by a smeared $1/\nu$ absorber in the top reflector.

^bCentral processing units (computer time) on IBM 3033.

^cThis is the k_{eff} difference between two central rod insertions.

the need of inserting a large portion of the rods into the core is of key interest. Additionally, the applicability of Monte Carlo theory to full-core, three-dimensional models is shown to be practical. Use of MCPSM, however, is not recommended for the particular applications of this study. Details of this study are described elsewhere.¹⁰

7.4.2 Cross Sections Versus Burnup for Pebble-Bed Reactor Core — B. A. Worley

The 1640-MW(t) PBR for direct-cycle application using low-enriched uranium fuel was modeled in one dimension, and a steady-state solution was calculated for an initial reference cross-section set. Beginning-of-life (BOL) and end-of-life (EOL) nuclide number densities were used to

Table 7.5. Reactivity worth of 85 control rods
in a 1640-MW(t) Pebble-Bed Reactor

Case	Rod position, cm (0.0 = top of bed)	k_{eff}	Number of histories	CPU ^a (min)
A	+200.0 (out of top reflector)	1.01598 $\pm 0.00139^b$	150,000	103.8
B	+100.0 (out of void, in top reflector)	0.99506 ± 0.00189	90,000	57.8
C	0.0 (top of bed)	0.96175 ± 0.00176	90,000	111.9 ^c
D	-93.5 (in 3 fuel zones)	0.91734 ± 0.00157	90,000	57.0
E	-187.0 (in 6 fuel zones)	0.88680 ± 0.00116	150,000	104.5
F	-405.2 (in 13 fuel zones)	0.80388 ± 0.00127	150,000	105.9
k_{eff} , rods in upper void (Δk_{eff} due to rods withdrawn out of the upper void)		0.96175 $\pm$ 0.00176 ^d (0.03331 $\pm$ 0.00258) ^e (0.02555 $\pm$ 0.00121) ^f		
		0.96144 $\pm$ 0.00166 ^g (0.02740 $\pm$ 0.00135) ^g		142.5

^aCentral processing units (computer time) on IBM 3033.

^bOne standard deviation.

^cThis is a perturbation source calculation for control rod movement out of upper void.

^dThe same calculation as case C above.

^eObtained by subtracting k_{eff} of case C from that of case B.

^fThe Δk_{eff} obtained from perturbation source method (MCPSM) of case C.

^gSame as case C with greater number of importance regions and more adjoint particles.

recalculate two sets of shielded collapsed four-group cross-section files. The k_{eff} was determined for the reference steady-state nuclide distribution by using the BOL and EOL cross-section files as well as a weighted combination of these two files. The results are listed in Table 7.6. In addition, the effect of cross-section correlation schemes on the calculated equilibrium fissile feed rate was determined, and results are listed. The requirements to produce accurate results and the magnitude of the inaccuracy associated with simple models are of interest in core analysis effort.

Table 7.6 Cross-section correlation results

Cross section file ^a	k_{eff}	Leakage fraction	Peak power density (MW/m ³)	²³⁵ U feed rate (kg/full-power day)	Fissile inventory (kg)	Conversion ratio
<i>Reference steady-state solution I^b</i>						
G.01d	1.0000	0.0480	10.41	1.75	807	0.599
G.1	9.9996	0.0474	10.16			
G.18	0.9788	0.0493	10.46			
G.1/G.18	0.9895	0.0493	10.87			
<i>Reference steady-state solution II (with $\alpha = 0.5$)^c</i>						
G.1/G.18						
$\alpha = 0.0$	1.0004	0.0475	10.52			
$\alpha = 0.5$	1.0000	0.0475	10.53	1.88	896	0.601
$\alpha = 1.0$	0.9991	0.0476	10.52			
$\alpha = 1.5$	0.9981	0.0476	10.51			
(G.1 + G.2)/2	0.9999	0.0476	9.97			
<i>Steady-state solutions III and IV</i>						
G.1	1.0000	0.0476	10.37	1.78	834	0.600
G.18	1.0000	0.0470	10.22	1.91	9.35	0.607

^aG.01d is initial reference cross section file from previous work; G.1 and G.18 refer to cross sections collapsed from nuclide densities of zones 1 and 18, respectively, from reference steady-state solution using G.01d; G.1/G.18 refers to a correlation between files G.1 and G.18.

^bSolution I is the steady-state solution using G.01d cross sections; solution II is the steady-state solution solved with a correlation of the cross sections from sets G.1 and G.18 with α (see note ^c) set to 0.5; solutions III and IV are steady-state solutions using cross-section sets G.1 and G.18, respectively. Solutions III and IV were obtained as reference limiting cases for comparison with the correlation used in solution II problems.

^cThe parameter α is used to correlate a nuclide cross section between two reference conditions representing two fuel burnups [MWd(t)/kg heavy metal] B_1 and B_2 ; for $\sigma(B_1)$ and $\sigma(B_2)$ representing two values of the cross section at burnups of B_1 and B_2 , $\sigma(B) = \sigma(B_1) + x[\sigma(B_2) - \sigma(B_1)]$ where $x = \tan^{-1}(\alpha\gamma)/\tan^{-1}(\alpha)$ and $\gamma = (B - B_1)/(B_2 - B_1)$.

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8 HIGH-TEMPERATURE REACTOR SHIELDING STUDIES

C. O. Slater and S. N. Cramer

8.1 INTRODUCTION

The High-Temperature Reactor (HTR) shielding studies work was directed toward the design and performance of an experiment to confirm calculated thermal neutron fluence levels in the High-Temperature Gas-Cooled Reactor (HTGR) lower regions. As a starting point, we performed design survey calculations [for the 900-MW(e) steam-cycle HTGR with a specific power of 7.1 MW/m^3] to obtain estimates of the thermal neutron fluence and to determine how close these fluence values are to the design limit. The results of the design survey calculations are summarized in Sect. 8.2. More detailed information is reported elsewhere.¹ Work on the design and preanalysis of the experiment is summarized in Sect. 8.3.

8.2 ANALYSIS OF RADIATION LEVELS IN THE HTGR LOWER REGIONS

8.2.1 Preliminary Radiation Level Estimates

Preliminary radiation level estimates were obtained from a two-dimensional (2-D) radiation transport calculation of a homogeneous model of the HTGR lower regions. The calculation was performed with the DOT-IV 2-D radiation transport code.² For this calculation, the effects of radiation streaming through the large coolant holes in the bottom reflectors and support blocks were not considered but were left to later calculations. The main concern was the thermal neutron fluence in the thermal barrier cover plate for the HTGR sidewall. A design limit of $10^{21}/\text{m}^2$ has been imposed. Thermal neutron fluence results from this calculation, shown in Fig. 8.1 as isofluence contours on the 2-D geometry model, show a maximum fluence in the sidewall thermal barrier cover plate of about $10^{20}/\text{m}^2$ at core level. For the portion of the sidewall thermal

Fig. 8.1 Isoplot of the thermal ($<3.05 \text{ eV}$) neutron fluence (cm^{-2}) in a homogeneous full-geometry mockup of the HTGR lower regions.

barrier cover plate beneath the core support block elevation, the maximum thermal neutron fluence was between 10^{18} and $10^{19}/\text{m}^2$. The fluence level in the sidewall at the core level should not be influenced by neutron streaming in the bottom reflectors and support blocks, and hence any streaming correction should not increase the expected fluence level of $10^{20}/\text{m}^2$. In the lower regions, however, a streaming correction factor greater than 100 could be tolerated before the design limit of $10^{21}/\text{m}^2$ would be exceeded.

8.2.2 Streaming Effects of Small Coolant Holes and Boron Pins

Because of the large number of small coolant holes and boron pins, exact mockups of regions containing them are extremely difficult to achieve. Therefore, the streaming effects of these regions were evaluated separately, and sources were corrected by the calculated factors to account for the streaming. The upper boron pin layer, which contained both small coolant holes and boron pins, was calculated with (1) an ANISN 1-D slab homogeneous model,³ (2) DOT 2-D homogeneous and heterogeneous models, and (3) MORSE exact models for an isolated hex block.⁴ The results showed small streaming effects for the small coolant holes, but thermal neutron streaming among the boron pins resulted in a factor of 4.0 increase in the thermal neutron leakage from a typical seven-column region. (Both a central control and an outer hex block were calculated with MORSE, and the results were combined to give an average streaming correction for the seven-column region.) These streaming-correction factors were applied to a boundary source 80 mm below the upper boron pin layer, and the source was used in the MORSE calculation of streaming through the large coolant holes. The factors were not applied to the source for the DOT calculations.

8.2.3 Streaming Effects of Large Coolant Holes in Bottom Reflectors and Support Blocks

Streaming-correction factors for the large coolant holes in the bottom reflectors and support blocks were obtained from the results of the DOT full-model homogeneous calculation and DOT and MORSE calculations of a typical seven-column region (cell) from about 80 mm below the top boron pin layer, through the bottom of the support blocks, and through a 0.35-m-thick homogeneous mockup of the support post region. The support post region mockup was included in the cell calculations to try to account for the increased leakage from the support block due to neutron back-scatter from the support posts and subsequent leakage from the support blocks. The inclusion of the support post mockup in the cell model also makes the leakage results compatible with those from the full homogeneous model. The results showed a thermal neutron streaming-correction factor of about 1600. Epithermal neutron streaming-correction factors ranged

from about 440 to about 8400. These factors are greater than the factor of 100 that could be tolerated on the basis of results reported in Sect. 8.2.1. A DOT calculation made with the streaming-correction factors applied to a boundary source at the bottom of the support block resulted in a maximum thermal neutron fluence of $4.2 \times 10^{21}/\text{m}^2$ in the sidewall thermal barrier cover plate. The design limit would be exceeded by a factor of 4.2 according to this calculation.

8.2.4 Streaming Effects of the Core Support Region

The core support posts have diameters of about 0.23 and 0.28 m and are clustered in groups of three under support floor segments. The resulting pattern has relatively large open areas among neighboring regions and at the center of a given region but relatively smaller open areas among the three support posts at the corners of three neighboring regions. The open areas allow for additional neutron streaming, which was not properly accounted for when the support post region was represented as a reduced-density homogeneous graphite medium. The MORSE Monte Carlo code was used to calculate the neutron transmission through the support post region, and the streaming factors were evaluated by comparing the MORSE results with DOT results. The MORSE calculation used a streaming-corrected source at the bottom of the support blocks. Neutron fluxes were calculated for cylindrical surface segments at the bottom of the support posts and on the sidewall thermal barrier cover plate. Average fluxes for these same segments were calculated from output fluxes from the DOT calculation with the streaming-corrected source. A comparison of the MORSE and DOT results showed additional thermal neutron streaming corrections of 2.2 to 2.8 at the bottom of the support posts and 1.5 to 2.6 at the sidewall. The epithermal neutron streaming-correction factors ranged from 6.2 to 10.2 at the bottom of the support posts and from 1.2 to 6.5 at the sidewall.

8.2.5 Final Radiation Level Estimates and Design Implications

From the calculated support post streaming-correction factors, 2-D arrays of streaming-correction factors were formed (factors assumed to be unity a short distance above the bottom of the support block) and applied to the output fluxes from the DOT streaming-corrected lower region

calculation. The resulting thermal neutron fluence values are plotted in Fig. 8.2 as isofluence contours on the 2-D geometry. The maximum thermal neutron fluence in the sidewall thermal barrier cover plate is now found to be $1.05 \times 10^{22}/\text{m}^2$ or 10.5 times the design limit. This value is somewhat uncertain and probably conservative. It does indicate that additional shielding or a different shielding arrangement will be required to meet the current design limit. Because of the uncertainties associated with the calculations, a comparison of calculated and measured results for a prototypic HTGR lower region geometry seems imperative.

Fig. 8.2. Isoplot of the streaming-corrected 32 full-power-year thermal neutron fluence (cm^{-2}) in the HTGR lower regions.

8.3 BOTTOM REFLECTOR AND SUPPORT BLOCK NEUTRON STREAMING EXPERIMENT

After sufficient work had been completed on the survey analysis of radiation levels in the HTGR, work was begun on the design and preanalysis of the HTGR Bottom Reflector and Support Block Neutron Streaming Experiment. The first task involved the design of a spectrum modifier that would give a spectrum similar to that at a selected location in the HTGR bottom reflectors.

The spectrum modifier calculations were performed with the ANISN 1-D transport code. The source at the surface of the Tower Shielding Reactor (TSR) was assumed to be incident on the left face of slabs of material. The S_{16} quadrature in which the TSR source was given was used in the calculation. The source was collapsed from its original 51 groups to 15 groups by weighting with lethargy. Cross-section scattering matrices up to P_3 were used.

Of the several combinations of materials tried, the combination that gave a spectrum most closely resembling the HTGR design spectrum with which it was compared was one consisting of 50 mm of carbon steel followed by 195 mm of graphite. The spectrum from the spectrum modifier bore close resemblance to that at the interface between the HTGR core and bottom reflector, even for the thermal flux. The modified spectrum is compared with spectra at the core-reflector interface and above the top boron pin layer in Fig. 8.3. Although there was some motivation for producing a spectrum that matched the design spectrum above the top boron pin layer, we decided to use this particular spectrum modifier because of its similarity to the design spectrum at the core-reflector interface.

A 2-D DOT calculation, which included the TSR geometry and a spectrum modifier consisting of 50.8 mm of carbon steel followed by 203.2 mm of graphite, was then performed to obtain a source for the DOT calculations of the experimental configurations. The 2-D geometry for the calculation is shown in Fig. 8.4. The calculated spectrum emerging from the spectrum modifier is compared with the design spectrum in Fig. 8.5. Here we note that the thermal neutron flux fraction appears to be less than that calculated with the 1-D modeling (see Fig. 8.3). The epithermal neutron flux spectrum matches the design spectrum well, but the thermal neutron flux

Fig. 8.3. Comparison of HTGR design spectra with a one-dimensionally calculated Tower Shielding Reactor spectrum modified with carbon steel and graphite.

appears to be one-third the design value if the two curves are normalized to the same value around 500 keV. An isoplot of the thermal neutron flux superimposed on the 2-D geometry is shown in Fig. 8.6. The thermal flux at the exit of the spectrum modifier (top of Fig. 8.6) was about 27% of the total neutron flux. This contrasts with the 52% of the total flux at the interface between the HTGR core and the bottom reflector and 63% of the total flux above the top boron pin layer. Thus, the thermal neutron flux makes up a smaller part of the total flux for the experiment than it does for the HTGR design. As a result, the experimental results may indicate that the boron pin shields are less effective than they would be for the HTGR. For instance, in the HTGR design calculations, thermal neutron streaming among the boron pins resulted in a streaming-correction factor of 4.0, meaning that 75% of the thermal neutron flux at that location was due to thermal neutron streaming through the boron pin layer and that 25% was due to the thermalization of epithermal neutrons and the subsequent transport of the thermal neutrons to that location.

Fig. 8.4. Two-dimensional geometry for DOT Tower Shielding Reactor source calculation.

Starting with one-third the thermal neutron flux and the same epithermal neutron flux would probably lead to a streaming-correction factor of 2.0. The smaller streaming-correction factor means that the boron pins appear to be more transparent and thus less effective for shielding than in the design case.

The other work on the experiment included the drafting of and outline of the experimental configurations and the measurements to be taken. In addition, work was begun on the 3-D geometry model for the experimental configuration containing the first boron pin layer. The calculation is to be performed with the MORSE Monte Carlo radiation transport code.

Fig. 8.5. Comparison of the DOT-calculated Tower Shielding Facility modifier spectrum with the HTGR design spectrum at the core-reflector interface.

Fig. 8.6. Isoplot of the thermal neutron flux ($\text{cm}^{-2} \cdot \text{min}^{-1} \cdot \text{W}^{-1}$) for the Tower Shielding Reactor source calculation for the HTGR shielding experiment.

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9. HIGH-TEMPERATURE REACTOR COMPONENT FLOW TEST LOOP STUDIES

J. P. Sanders, A. G. Grindell, and W. P. Eatherly

9.1 INTRODUCTION

Construction of the Component Flow Test Loop (CFTL) was completed to a designated Stage Alpha under the Gas-Cooled Fast Reactor (GCFR) program (Budget Activity Code AF 10 15) during FY 1981. An isometric drawing of the central loop is shown in Fig. 9.1, and the features of both the Stage Alpha and the Stage I (a previously proposed level of construction) modifications are given in the System Design Description¹ for the CFTL.* This document also describes the original features for the overall loop and the ancillary equipment.

The Stage Alpha configuration has a piece of 4-in. pipe replacing the test vessel that had been designed for the original CFTL. The absence of the test vessel and its test bundle resulted in the deletion of electrical connectors between the loop area and the power supply and the deletion of all the power controllers except a 0.5-MW unit that had been purchased early in the project. For Stage Alpha, the second air-cooled heat exchanger and its associated helium piping were eliminated; however, the air supply for this unit was installed. The helium piping including a vortex shedding flowmeter for the attemperation flow of helium was eliminated, and the amount of instrumentation that was installed was limited to the part that is necessary for loop control. The Data Acquisition System (DAS) was installed; however, no instrument signals were connected to it.

The progress of the final construction activities is described in detail in the closeout progress report for the GCFR Program at ORNL.² Some components, on order for the CFTL but not required for Stage Alpha, had not been received at the end of FY 1981. In addition, instabilities

*Known as Core Flow Test Loop before May 1981.

Fig. 9.1. Main loop piping for the Stage Alpha Component Flow Test Loop.
Dotted lines indicate major components eliminated in the Stage Alpha modification.

in the position indicators for the gas-bearing surfaces on the three circulators precluded the full range of acceptance testing. This required a modification of the directive to continue the closeout activities into FY 1982. Final shakedown testing and completion of Title III design were accomplished by the end of December 1981.

Funds were added to the CFTL task during FY 1981 under the HTGR Program to make modifications that would improve the capabilities of the loop to evaluate the performance of components for the HTGR. These modifications included a revision in the installation of the piping to provide downflow at the test piece, purchase of 1 MW of additional power controllers, and extended testing of the gas-bearing circulators by the manufacturer to prove their capabilities for operation at higher temperatures than previously specified.

These funds were also used to investigate the most appropriate application of the loop to the test of HTGR components. This investigation indicated that the most effective use of the facility would be to study the engineering performance of primary system components at design temperatures and pressures. Previous studies had indicated the need to determine the performance of the graphite core support structure for the prismatic core.³ The Core Support Performance Test would use a modification of the previous test vessel design to determine the performance of representative full-size components.

Investigation of other components in the HTGR system was continued to identify items vital to system performance and safety that can be evaluated later or simultaneously to make optimum use of the loop capabilities. These include the Core Auxiliary Heat Exchanger, a helium-to-helium intermediate heat exchanger, a reformer, primary system mechanical components, and the thermal barrier structures.

This chapter describes these CFTL activities and other activities relative to the future testing of HTGR components.

9.2 HIGH-TEMPERATURE REACTOR APPLICATION STUDIES — J. P. Sanders

In evaluating the application of the CFTL to the design data needs of the HTGR Program, the most effective use of the experiment facility

appeared to be investigating the engineering performance of primary system components at the proposed operating limits of temperature and pressure.

The characteristics of this loop that distinguish it from similar facilities available to the High-Temperature Reactor (HTR) program are its capability to circulate helium at HTR operating temperature and pressure and to execute rapid and precisely controlled transients for both flow and power input. Another unusual attribute of the loop is the availability of a sophisticated DAS that can collect as many as 10,000 data points per second from some 640 addressable channels.

For the application of the loop to the evaluation of HTR system components, a major feature of the site is the availability of a reserve of electrical power. At present, transformers that can supply 6.75 MW at required system voltages have been installed within the building. In addition, transformers installed just outside the building can supply 40 MW at 13,800 V. The local supply system makes amounts of power available on either a maximum or continuous use basis that is not duplicated at other sites.

The initial study of areas of application of the CFTL capabilities to HTR development indicated that the following features should be considered in detail:

1. the lifetime performance characteristics of the core support structure;
2. the performance of flow control devices in the primary loop;
3. the performance characteristics and structural response of heat exchange equipment such as
 - a. the primary steam generator,
 - b. the intermediate heat exchanger associated with an indirect cycle,
 - c. the core auxiliary heat exchanger,
 - d. the reformer in a primary or secondary loop, and
 - e. the methanator associated with the reformer;
4. the performance of the thermal barrier; and
5. the effect of flow reversals on flow redistribution in the core due to natural convection effects.

When the efforts were directed to the range of tests involved in the initial investigation, attention was given to the following restraints on the overall test plan:

1. Primary consideration would be given to those applications that would make maximum use of the existing hardware and capabilities of the loop.

2. Overall plans should accommodate both near-term, short-range tests and tests that require a longer period to acquire the necessary test model and to make the needed loop modifications.

3. Testing should be completed in a timely manner so that the results can feed into the design of the proposed demonstration plant.

4. Testing will be directed primarily toward those components that are generic to all applications of the HTR plant.

5. Preference will be given to the experimental evaluation of those components that affect the system design in the following areas: (a) safe operation, (b) reliability and availability, (c) uncertainty in design limits, and (d) optimization of performance.

6. Other available test facilities will be considered, and preference will be given to the specification of those tests for which the CFTL has characteristics that are more applicable to the test needs.

7. The total needs of experimental evaluation of HTR components will be considered, and those choices that can most easily be integrated into an overall test program will be made.

9.2.1 The Loop Configuration

The flowsheet for Stage Alpha CFTL, which has now been designated as the Component Flow Test Loop for the HTR, is shown in Fig. 9.2. This represents the construction efforts that were included in the closeout efforts for the GCFR program. Relative to the requirements for the proposed core support performance test, the significant attributes are (1) the test vessel was replaced with a section of 4-in. pipe, (2) the attemperation flow line was not installed, (3) the controller for the power supply had a maximum capacity of 0.5 MW, and (4) no signal leads were connected to the DAS.

A number of other components have been received but either are not installed or are not connected in Stage Alpha. These components include (1) the parts for the test vessel, (2) two calibrated vortex shedding flow meters, (3) transformers to supply approximately 4.5 MW of additional

Fig. 9.2. Flowsheet of the Stage Alpha Component Flow Test Loop.

power for the test section, (4) the cooling air supply for a secondary helium heat exchanger, and (5) a supply of components for the electrically heated rods that were to be used to simulate the GCFR fuel rods.

An isometric drawing (Fig. 9.1) illustrates the relative locations of those loop components installed for Stage Alpha. An overall isometric (Fig. 9.3) shows the relationship of the loop to ancillary equipment such as the power supply, the DAS, and the cooling air supply system for the helium heat exchangers. Additional equipment that provides cooling air for the transformers and the power controls is not shown.

Stage Alpha impurity measurements were to be performed by off-line analysis of batch samples. Impurity control was accomplished by helium venting and makeup. For the HTR testing, the loop must be modified for continuous on-line impurity monitoring, purification, and injection. Such a modification was part of the original design for the CFTL; however, the required components were deleted from the construction plans for Stage Alpha.

Fig. 9.3. Overall isometric view of the Component Flow Test Loop (Stage Alpha) showing the relative location of ancillary equipment.

9.2.2 Loop Limitations

The metal pressure boundary of the test vessel and piping has a maximum operating temperature of 600°C (1100°F) at the design pressure. To limit the temperature of this boundary and still provide a test environment at HTR conditions, we plan to divert part of the inlet helium for attemperation flow around any test assembly. This attemperation flow is then mixed with the outlet helium from the test assembly to produce a mixed mean temperature of 600°C or below before it contacts the pressure boundary of the loop.

An air-cooled heat exchanger is installed in the loop just downstream from the test assembly, as shown in Fig. 9.2. This unit will remove the heat added by an electric heater and reduce the gas temperature to the maximum allowable temperature for the helium circulators. The original design criteria for the CFTL circulators specified a maximum allowable temperature of 345°C (650°F), which was based on the thermal limit of the electrical insulation in the motor windings. Initial testing of the prototype circulator indicated that the motor cooling capacity maintained a significantly lower temperature in the windings than was determined in the design calculations made by Mechanical Technology, Inc. That company's testing of the circulator was extended to establish a maximum allowable operating gas temperature of 450°C (850°F).

The major operating limit that will impact the application of the loop to the HTR program is the capacity of the circulator. At the initial operating conditions for the circulator,¹ which were 10.3 MPa (1500 psia) and 350°C (660°F), the available head of the three circulators in series was 73 kJ/kg (85.5 psi). At a system pressure of 7.24 MPa (1050 psia at 345°C) for the HTR design the head will be 73 kJ/kg (51.0 psi at 450°C). The initial mass flow was 3.2 kg/s (7 lb/s) at 10.3 MPa; at 7.24 MPa this flow will be 2.2 kg/s (4.9 lb/s) since the circulators can be considered to have constant volumetric flow over this range of conditions.

This mass flow will be divided into a test assembly flow and the attemperation flow used to limit the temperature of the vessel wall. The ratio of flow in these two streams will be determined by the temperature of the exit gas from the test; this temperature will also dictate the

power requirement of the electrical heater. The geometry of the test assembly will establish the heat and mass transfer coefficients in the test piece and the pressure drop through this section.

9.2.3 Core Support Performance Test

According to the foregoing considerations and the loop attributes, the test of the greatest importance whose requirements match the CFTL capabilities was a performance test for the proposed core support structure for the prismatic HTGR core. Preliminary evaluation of the test requirements indicated that the flow range, power input, and available test bed volume match closely the capabilities of the Stage Alpha CFTL. It also appeared that other hardware that had been acquired for the CFTL but not installed in the Stage Alpha could be used to fabricate an appropriate test vessel.

The requirements for the test were based on a study of the core support structure that had been made at Oak Ridge. The results were summarized in a report prepared by Mechanics Research Inc. (MRI) as a subcontractor for this study.⁴ The major concern indicated by this study was the structural interaction at the interface between the core support post and its seat.

As shown in Fig. 9.4, different radii are specified for the rounded end of the post and the face of the seat. This difference is necessary to allow movement in the core support structure relative to the prestressed concrete reactor vessel floor during thermal transients and during seismic events. This design creates a limited area of contact and results in stress concentrations (Hertzian stresses).

The structural performance of the post and seat also depends on the corrosion (or burn-off) of the graphite by low concentrations of water vapor or other sources of oxygen in the circulating gas. There were concerns about the correlations for the burn-off rate at the time of the MRI study. In addition, subsequent studies have indicated some uncertainty concerning the volumetric distribution of the burn-off effect.⁵⁻⁷ These tests with small specimens presented some questions about whether the burn-off occurs mainly at the surface of the graphite, uniformly throughout

Fig. 9.4. Typical section of HTGR core support structure indicating curved ends of the core support posts.

the graphite piece, or perhaps primarily in the interior of the specimen. The predictions do not include the effects on the ultimate strength of the members as a function of burn-off.

Because of this concern about the rate of burn-off, the distribution of burn-off, and the effect of burn-off on strength, ORNL had previously recommended³ evaluating the performance of the core support post in a full-scale test under accelerated corrosion conditions. It now appears feasible, through relatively minor modifications of the CFTL, to test a full-size segment of the core support structure. This test would incorporate the three types of graphite found in the support blocks, the post seats, and the support posts.

This assembly would be structurally loaded and tested under normal operating conditions, conditions corresponding to the limits imposed by the operating technical specifications, and certain accelerated corrosion

conditions. This testing sequence, which might involve several test structures, was designated as the Core Support Performance Test and is discussed in more detail in Sect. 9.3.

9.2.4 Testing of Heat Exchanger Equipment

As tests following those for the core support structure, we have considered the determination of critical performance characteristics of the heat exchange equipment in the HTR. The vital questions that could be answered in tests adapted to the CFTL concern flow distribution and pressure loss in the equipment, surface heat transfer characteristics, and temperature distributions and related stresses during transient operation. Items of equipment include the intermediate heat exchanger (IHX) that is generic for all indirect-cycle HTR designs, a steam generator design that differs significantly from the Fort St. Vrain design, the Core Auxiliary Heat Exchanger (CAHE), and a reformer plus its associated methanator unit.

9.2.4.1 Intermediate Heat Exchanger

The test of a representative section of an IHX was considered. The objective is to determine the adequacy of materials and structural design, to obtain the heat exchange data and related correlations, and to assist in optimizing the design of this component. The IHX was studied as the first large component to be tested since it is generic to all advanced HTR designs using indirect cycles. In addition, it is representative of a group of components of similar design such as steam generators, reformers, and recuperators. In developing various applications of the HTR, the IHX will be required for the high-temperature process heat or reformer application. A lack of confidence in the heat transfer and flow data for helium in heat exchangers at flow patterns and geometries proposed for the IHX will result in overdesign and will impact the cost of other components such as the reactor vessel. Optimizing the design of helium heat exchangers appears to be very cost effective.

Lack of performance characteristics of materials, tubesheet structures, and tube support plate structures requires reduced-scale or segment testing of the design before its installation in an HTR. The reliability of the performance of these parts for its lifetime is vital to licensability, and this information can be gathered only through realistic testing.

The objectives of the IHX testing would be as follows:

1. to explore and determine the adequacy and reliability of the HTR heat exchanger design under normal operating conditions and under off-design conditions including transients, cycling, and depressurizations;
2. to gather systematic heat transfer and flow data that can be used to optimize HTR heat exchanger designs for normal and off-normal conditions;
3. to test large sections of the IHX with prototypical dimensions to confirm the material behavior;
4. to determine failure modes of the IHX and their impact, their detectability, and the associated maintenance requirements.

Sections of an IHX with prototypical dimensions would be instrumented with temperature, stress, vibrational, and acoustic sensors. The sections would be operated in the CFTL with circulating helium at various pressures, temperatures, and coolant impurity levels. Flow, temperature, and pressure cycling and transients as well as impurity levels would be controlled. The data acquired would be analyzed to confirm and improve design and performance characteristics. Some design parameters such as tube diameter and tube pitch would be varied to assist in the selection of optimum design features.

The IHX has helium flow on both sides of the tube walls. Several loop configurations are possible; the choice will depend on the size of the test section or model and the energy requirements for testing. Two configurations are shown schematically in Figs. 9.5 and 9.6. Those tests that require an inlet temperature to the IHX greater than 450°C need a preheater to increase the allowable outlet temperature from the CFTL circulators. The design of these circulators limits their operation to this maximum gas temperature. Attenuation flow will be required in many tests to reduce the temperature of the helium leaving the IHX to about 600°C to protect the large heat sink.

In the dual loop arrangement, shown in Fig. 9.5, the IHX is installed with two independent loops. The main loop will use the large heat sink and requires a preheater. A bypass flow will bring the discharge gas to the thermal limits of the loop piping. The secondary loop will use one of

ORNL-DWG 81-7811 ETD

Fig. 9.5. Proposed dual loop arrangement for the Component Flow Test Loop for use in evaluating the performance of heat exchange equipment.

ORNL-DWG 81-7812 ETD

Fig. 9.6. Proposed "figure eight" design for the Component Flow Test Loop to evaluate the performance of helium-to-helium heat exchange equipment.

the circulators and a small heat sink to reduce temperatures to the operating range of the circulator. The advantage of this arrangement is the ability to vary flow, pressure, and temperature in each loop independently. The secondary coolant can be changed to include other gases.

The disadvantage of this arrangement is that it requires two separate sets of circulators, additional components and controls, and greater expense. Also, the total size of the test apparatus is limited by the number of circulators, the power supply, and the heat sink.

In the "figure eight" installation, shown in Fig. 9.6, the same helium flow passes through both sides of the IHX. The helium from the circulators is preheated to the temperature required for the inlet to the IHX primary side. At the exit of the primary side a heat source increases the temperature of the helium before it enters the secondary side of the IHX. At the exit of the IHX, the secondary flow of helium is mixed with attemperation flow to reduce the temperature as necessary to protect the heat sink.

The power load of the heaters and the duty of the heat sink are approximately equal for steady-state operation. The actual duty of the IHX may be as much as ten times the duty of the ancillary units for the figure eight arrangement. Thus, the figure eight arrangement permits the testing of larger units and requires fewer modifications of the loop than the dual loop arrangement. However, in this arrangement the pressures of both sides and to some extent the temperatures are inter-related. The pressure difference of the two sides of the IHX is limited by the available head of the circulators. Flow transients on either the primary or secondary side can be represented by bypass streams. Other transients, cycling, and depressurizations are limited by the combined dynamics of the system.

9.2.4.2 Steam Generator

A significant amount of testing was performed relative to the development of the steam generators for the Fort St. Vrain design; however, significant changes have been made in the basic concept for the unit for large HTRs. The reheater unit has been eliminated, the helium

flow path has been changed, and the superheater section is considerably different. Subsequent testing will be required before the construction of large HTR steam-cycle plants.

Testing a steam generator module would require the design, procurement, and fabrication of a dual-cycle CFTL similar to the diagram in Fig. 9.5. An appropriate feedwater system and a condenser would replace the circulator and heat sink in the secondary cycle.

9.2.4.3 Core Auxiliary Heat Exchanger

The CAHE is another unit that requires experimental verification of its performance. No similar unit was installed in the Fort St. Vrain plant; the main steam generators were flooded to serve this purpose. Also, the design of the CAHE bundle has been modified significantly since the detailed design of the Fulton and Delmarva plants. The bundle now consists of reentrant (bayonet) tubes located at the lower face of the reactor vessel.

Because of the significance of the performance of this unit relative to the safety of the reactor, detailed experimentation of anticipated operating conditions will be required. During the year, General Atomic Company (GA) proposed two different model tests for the current CAHE reentrant tube design. One model would be a 19-tube full-size segment of the bundle that would operate at the maximum temperatures that would be predicted for all transient conditions. The second model would be a one-sixth bundle that would operate at more limited temperatures. The second model would be used to determine pressure losses and flow distribution in the bundle, and the first bundle would be used to determine the effect of the transport of heat from the gas to the tube wall due to radiation from the gas at high temperatures.

The possibility of performing the required tests in the CFTL has been analyzed, and a preliminary cost estimate has been prepared for each test model. The required operating conditions are summarized in Table 9.1. Because the circulators in the CFTL are essentially constant-volume machines, the required mass flow could not be delivered at the depressurized (i.e., low-density) conditions. For the 19-tube test model, the required flows could be obtained if the three circulators were rearranged

Table 9.1. Test conditions for proposed Core Auxiliary Heat Exchanger bundles

	Operating conditions for each model					
	Pressurized			Depressurized		
	Prototype	19-Tube	120-Tube (1/6 bundle)	Prototype	19-Tube	120-Tube (1/6 bundle)
<i>Gas-side conditions</i>						
Fluid	Helium and water	Helium and water	Helium and water	Helium and air	Helium and air	Helium and air
Molecular weight	5	5	5	12	12	12
Mixture C_p , J/(kg·K)	4390	4390	4390	1970	1970	1970
Btu/(lb·°F)	1.05	1.05	1.05	0.47	0.47	0.47
Mass flow, kg/s	35.8	0.945	5.97	17.66	0.466	2.95
lb/h	2.844 E+5	7.50 E+3	4.74 E+4	1.402 E+5	3.70 E+3	2.34 E+4
Inlet temperature, °C	860	860	860	950	950	950
°F	1580	1580	1580	1750	1750	1750
Outlet temperature, °C	362	362	362	320	320	320
°F	683	683	683	612	612	612
Inlet pressure, MPa	7.24	7.24	7.24	0.163	0.163	0.163
psia	1050	1050	1050	23.6	23.6	23.6
<i>Water-side conditions</i>						
Mass flow, kg/s	141.1	3.73	23.56	141.1	3.73	23.56
lb/h	1.12 E+6	2.96 E+4	1.87 E+5	1.12 E+6	2.96 E+4	1.87 E+5
Inlet temperature, °C	150	150	150	80	80	80
°F	305	305	305	177	177	177
Outlet temperature, °C	284	284	284	118	118	118
°F	544	544	544	244	244	244
Pressure, MPa	10.3	10.3	10.3	10.3	10.3	10.3
psia	1500	1500	1500	1500	1500	1500
Heat duty, MW	78.5	2.1	13.1	21.9	0.6	3.7

so that they would operate in parallel rather than in series, as they are currently configured. For the one-sixth bundle model, even the three circulators in parallel would not provide sufficient flow.

The loop could meet all the required operating conditions for the 19-tube bundle if a minimum pressure of 480 kPa (70 psia) were specified for the simulation of the depressurized condition. Accepting this restriction would reduce the cost of implementing the CAHE test from \$1.978 million to \$1.375 million, based on preliminary cost estimates. Putting the three circulators in parallel would supply the required flow for the 19-tube bundle under the depressurized condition. However, the circulators in parallel would not supply the required mass flow for the one-sixth bundle at the depressurized condition of 163 kPa (23.6 psia). To supply the required mass flow, the minimum system pressure would have to be 1000 kPa (147 psia).

9.2.4.4 Reformer and Methanator

Testing the reformer, which has been proposed as an application for HTRs, would also require a dual-loop arrangement similar to that shown in Fig. 9.5. For this test the secondary loop would circulate the makeup and effluent gas for the reformer. This loop could be operated as an open loop or as a closed loop containing the methanator. In addition to the heat transfer and flow characteristics of the heat transfer surfaces, information is needed concerning the performance of the catalyst when operated at simulated HTR conditions. In particular, the ability of the methanator cycle to perform when subjected to the normal transients created by HTR operation must be investigated before this process can be incorporated into a demonstration facility.

An application of this type is considered as a long-range possibility for use of the CFTL.

9.2.5 Other Primary Loop Components

Several other components of the primary loop of an HTR could be tested in the CFTL. Each of these applications would require the construction of a relatively large test vessel in the existing loop.

9.2.5.1 Thermal Barrier

Because of the vital function of the thermal barrier that protects the prestressed concrete reactor vessel liner from the hot gases in the primary loop, several aspects of the design and performance of barrier components should be tested in the CFTL. To represent the actual geometry of the barrier, relatively large test vessels would be required. Aspects of the insulation subjected to the test would be the structural performance of the Class A and Class B cover sheets at the limiting helium temperatures and the structural performance of the liners for the cross ducts connecting the lower plenum to the steam generator cavities. Determination of the performance as a function of time at temperature would be required.

Such testing is within the capability of the CFTL if large "flow blockers" can be placed in the vessel to create the required velocities at the surface of the test piece.

9.2.5.2 Intracore Components

The need for a preoperational flow test on the central core cavity components has been emphasized by the flow oscillations encountered in the initial operational phases of the Fort St. Vrain Reactor plant. In addition to exposure at conditions at normal operating ranges, it is important to examine the flow and thermal response for severe accident sequences that follow the loss of forced convection cooling and coincident reactor trip. Tests should include the conditions following this incident in which low convective flows are imposed at varying intervals. This test would be vital in establishing the ability of a natural convection flow to remove afterheat.

Such a test would involve a large test vessel with a distributed internal heat source. Because of the necessity of representing a large number of flow channels at differing powers, an appropriately scaled model of the core could be selected. The resources for this test are available at the CFTL site.

9.2.6 Conclusions of Application Studies

From a preliminary evaluation of the attributes of the CFTL loop and the development needs of commercial HTR plants, we concluded that the

optimum choice for an initial test would be a study of the performance of the core support structure. This test would subject a series of test pieces of typical dimensions to a compressive load under conditions that would be encountered at the HTR core outlet. The concentration of impurities (mainly water vapor) would be increased so that the corrosion of the support during its 40-year plant life would be represented in a six-month test.

While this testing is in progress, detailed study of subsequent tests would be made. From the results of that study, the requirements for the next test would be established, and the design loop modifications, followed by procurement and installation of equipment, would proceed.

9.3 CORE SUPPORT PERFORMANCE TEST — J. P. Sanders

Iteration has continued with staff members at the General Atomic Company (GA) concerning the attributes of the proposed Core Support Performance Test (CSPT). The original scope and plans for the test were detailed in a presentation to the staff of GA in November 1980. As a result of this exchange of information, the following modifications were made to the original test plan:

1. The schedule was modified to permit installation of an impurity measurement and control system before the first test piece is installed in the test vessel.
2. Installation of the test vessel would be delayed until after completion and acceptance testing of the Stage Alpha CFTL. Final shake-down testing was completed in December 1981.
3. Plans to acquire and qualify graphite pieces have been included in the schedule and cost estimates.
4. The test vessel design was modified to facilitate the removal and replacement of the graphite test piece.
5. We considered including a mechanism to shift the loading on the test piece during the test sequence. This shift would represent the normal movement of the core support and shifts in loading following a possible seismic event. No plans were included to represent rapid changes in loading that might occur during a seismic event.

In continuing correspondence with personnel at GA during the year, the configuration of the test piece, the test sequence, and the level of impurities that would be maintained during the proposed six-month test period were discussed and reviewed. As a result of this interchange of information, a cooperative order was placed for the graphite that would be used in the test. The graphite received in this order will be divided between ORNL and GA, and qualification tests will be performed on representative samples.

Details of the determination of the required level of impurities that would make the six-month test period equivalent, both in amount and character, to the 40-year life of the core support structure in the reactor are discussed in the following section.

9.3.1 Graphite Procurement — W. P. Eatherly

The material currently scheduled for use in the core posts and pads is Stackpole 2020, selected for its fine texture, high strength, and overall uniformity. This material is, however, graphitized at only 2500 to 2600°C and is not manufactured to the close controls normally required for nuclear applications. As a result, the commercial grade shows considerable variability in impurity content, specifically calcium. The impure billets of 2020 show catalyzed oxidation, exhibited by "wormholing" at presumed calcium inclusions. As a consequence, a 2020-grade material manufactured to tight requirements is necessary for reactor use, and hence for the CSPT.

In conjunction with GA, a set of specifications was drawn up for such a material. In discussions with Stackpole Carbon Company, decisions were made to use selected raw materials of low impurity level and to graphitize at 2800°C or above to improve uniformity and increase evaporation of impurities. We know from experiments at ORNL that 2800°C is sufficient to remove calcium. This special production run is now in progress, and delivery of six full-size posts is expected in April 1982.

9.3.2 Test Operating Conditions — C. D. Bopp and W. P. Eatherly

A meaningful operating test in the CFTL has a number of strong constrictions. First, the test must be long enough to establish stable

oxidizing conditions with respect to both the gas concentrations and the material response to those conditions. Second, the test must be short enough to yield results soon enough and, above all, at reasonable cost. We decided rather arbitrarily to set the run time at six months and to assess the scientific and engineering significance of the test at that time level.

For these analyses, the validity of the so-called Gadsby mechanism is assumed. This results in the kinetic equation

$$R = kp_{H_2O}/(1 + k_1p_{H_2}^\alpha + k_2p_{H_2O}) ,$$

where R is the rate of carbon removal, p_i the partial pressure of the i th gaseous component, and k , k_1 , k_2 , and α are constants associated with various surface reactions. This equation leads to an oxidation model in which, in the absence of catalysis, the graphite is attacked at so-called active sites, which in turn may be partially blocked by sorbed hydrogen or hydroxyl radicals. The coefficients that we have employed for this equation are those developed by Valesquez et al.⁸ for H451 graphite but modified by adjusting the coefficient k as appropriate to uncatalyzed 2020 graphite. Obviously, at any given temperature one can obtain the same reaction rate R by altering the proportions of hydrogen and water.

The kinetics equation of course does not tell the full story. It represents the reaction rate only at the geometric surface of the graphite, where the material is exposed to the full gas composition. As one moves into the interior of the material, the composition of the gas phase will change by depletion of water vapor and increase in reaction products, specifically hydrogen. This is particularly true for grade 2020 with its fine texture and small pore size, and at elevated pressures, where the helium background gas inhibits interdiffusion.

To consider this aspect of the problem, interdiffusion coefficients in the gas phase were assumed to vary with pressure as predicted by kinetic theory. A simplified internal reaction-rate theory developed by Thiele⁹ was then employed. One assumes that the reaction rate R may be approximated in the form

$$R \approx kp_{\text{H}_2\text{O}} ,$$

and then proceeds to calculate the internal reaction rate as the water vapor content is depleted. For example, in an infinite slab of material of thickness d , this leads to an internal reaction rate at depth x of

$$\frac{R(x)}{R(0)} = \frac{\cosh h(d-x)}{\cosh hd} ,$$

where

$$h = k/D_e ,$$

and D_e is the diffusion rate into the material. The equations are easily modified for other geometries.

We can now estimate the total loss of material from the support post by knowing the concentrations in the gas stream and the diffusion rate of gas into the post interior. The question now becomes one of finding a gas composition that will accomplish in six months what is presumed to occur in 32 full-power years in the reactor, not only in terms of total mass removed but also in terms of penetration into the interior of the post. The freedom of adjusting the hydrogen concentration relative to the water vapor concentration enables us to do so.

The final results of these calculations are given in Table 9.2. The upper half of the table is the calculated behavior of the graphite over the reactor life if the reactor operated continuously with gas composition at its technical specification limit; Δw represents the total weight loss from the column, Δr the calculated change in radius due to total removal of material, and δ the calculated skin depth due to oxidation, that is, the depth at which the density of the bulk graphite will drop to $1/e$ of its value before oxidation. At 1000°C the oxidation in the reactor calculates to be almost entirely superficial, whereas at 700°C it transforms to a predominantly penetrating mode.

Table 9.2. Approximate loop conditions that simulate expected reactor oxidation

Temperature (°C)	Pressure		Intrinsic rate	[H ₂ O] (ppm)	[H ₂] (ppm)	Δw^a (kg)	δ^b (mm)	Δr^c (mm)	$\Delta r/\delta$
	(MPa)	(atm)							
In reactor for 32 years									
1000	4.7	46	High	0.2	2	7.0	0.03	3.9	130
1000	4.7	46	Low	0.2	2	2.1	0.11	1.1	10
1000	7.2	71	High	0.2	2	11.5	0.04	6.6	150
1000	7.2	71	Low	0.2	2	3.1	0.10	1.7	17
700	7.2	71	High	0.2	2	0.10	1.4	0.057	0.04
700	7.2	71	Low	0.2	2	0.047	3.2	0.027	0.008
In loop for six months									
1000	4.7	46	High	200	4000	7.2	0.59	4.0	6.8
1000	4.7	46	Low	50	250	2.2	0.45	1.2	2.7
1000	7.2	71	High	400	8000	12	0.63	6.8	11
1000	7.2	71	Low	100	500	3.6	0.51	2.1	4.1
700	7.2	71	High	20	100	0.10	2.2	0.057	0.026
700	7.2	71	Low	20	100	0.048	5.0	0.026	0.005

^aTotal weight loss from the column.

^bCalculated skin depth due to oxidation (depth at which the density of the bulk graphite drops to 1/e of its preoxidation value).

^cCalculated change in radius due to total removal of material.

The bottom portion of the table gives corresponding values for a six-month loop test. The gas compositions are first fixed to equal the total weight loss expected in the reactor, and from this family of compositions one is selected to give at least the dominant mode of oxidation. It can be seen that the mode (superficial or penetrating) can be reproduced sufficiently well to both validate the theory and reproduce the expected effect in strength loss.

The above calculations are uncertain by a factor of at least 3 because of uncertainties in the intrinsic graphite reaction rates. Hence, at this point, they establish only the validity of the CSPT concept and its extrapolative power. But the calculations do not establish the exact conditions under which the experiment should be conducted. At present, the Thiele theory on penetrating oxidation is being studied to either remove or bound the idealizing assumptions. On receipt of the experimental grade 2020 graphite in April 1982, both GA and we will determine its

actual oxidation rate at 0.1 MPa (1 atm) pressure. Further, we hope that data will be available from either or both the GA and KFA high-pressure oxidation systems to further establish the reaction rates. At that point, the above calculations will be redone in much greater detail.

9.3.3 Materials Compatibility — C. D. Bopp and W. P. Eatherly

Quite aside from the question of intrinsic graphite oxidation is the problem of material interactions within the CFTL that could spuriously affect the graphite reactivity. Current plans call for molybdenum heaters and alumina supports. Neither can be assumed to be inert to graphite. The problem of most concern to us at the moment is the transport of molybdenum to graphite through vapor-phase molybdenum oxide.

Thermodynamic calculations indicate the possible oxidation of the molybdenum heater rods under CFTL conditions according to the reactions

For an anticipated $\text{H}_2:\text{H}_2\text{O}$ ratio of 5, oxidizing conditions prevail up to 627°C (1161°F), and, for a $\text{CO}:\text{CO}_2$ ratio of 2, oxidizing conditions exist at all temperatures. Since $\text{MoO}_2(\text{s})$ is somewhat volatile, deposition of molybdenum downstream could influence the oxidation of graphite by either the direct reaction

or by catalytic means. Although this assessment indicates that molybdenum may not be a good choice as the heater for the test gas, it is also likely that the stated gas ratios (assumed to be the gas composition at low temperatures) cannot be held constant at high temperature because of the reaction

If this reaction approaches equilibrium at the lower temperatures, molybdenum oxidation by H_2O will be counteracted by reduction back to molybdenum by carbon monoxide [the reverse of reaction (9.2)]. Reaction (9.4) also increases the $CO:CO_2$ ratio so that molybdenum oxidation by CO_2 is now expected to occur only above $887^\circ C$ ($1628^\circ F$), but any MoO_2 thus formed may be reduced to molybdenum by the reverse of reaction (9.1). That is, the buffering effects of reaction (9.4) may permit the use of molybdenum heaters for the CSPT even though the cold-test gas compositions appear unfavorable.

Thus, a test plan has been derived to determine (1) if the oxidation of graphite is influenced by the molybdenum in upstream positions and (2) if reaction (9.4) is a factor in the oxidation of molybdenum. To do this, the oxidation of graphite specimens will be determined for a period of about six weeks at $700^\circ C$ in a test gas containing 100 ppm H_2 , 100 ppm CO , 50 ppm CO_2 , and 20 ppm H_2O based on 7.2 MPa (71 atm) helium (7100 H_2 , 7100 CO , 3550 CO_2 , 1420 H_2O μatm). A companion test will also be run in a separate test loop under the same conditions, except in this case molybdenum specimens will be positioned in several upstream positions to give metal temperatures corresponding to heater temperatures of $700^\circ C$ ($1292^\circ F$) at the inlet and about $1100^\circ C$ ($2012^\circ F$) at the outlet. The $1100^\circ C$ limit is the highest permissible temperature of our existing furnaces. These experiments are in preparation, and results should be available by early summer 1982. The apparatuses being used are those designed for the metal oxidation studies.

In a second set of experiments, the contact compatibility of high-purity alumina with molybdenum and graphite is being investigated. Specimens of HT molybdenum* have been placed in physical contact with Carborundum alumina and inserted in a Brew vacuum furnace at $1400^\circ C$ for long-term testing. This current test is inappropriate, however, because the alumina contains significant amounts of silica. Coupons of Coors alumina and high-purity graphite are being prepared and will be inserted into the furnace as soon as possible. We intend to run the test for a total elapsed time of six months with periodic examination of the materials.

*HT molybdenum is a proprietary product of pure molybdenum doped with certain nonmetals to raise its recrystallization temperature.

9.3.4 Gas Impurity Measurement and Control — R. E. Harper

The analytical techniques that would best satisfy the needs of the CSPT were identified. A study of impurity injection and control techniques was initiated. Most of the work focused on the analytical instrumentation.

The salient criteria for the analytical instrumentation were high accuracy, capability of automatic and unattended operation, good reliability and maintainability records, and cost-effectiveness. We determined that an automated process gas chromatograph, as diagrammed in Fig. 9.7, would be suitable as the core of the analysis system. In the CSPT, the gas chromatograph will be used to measure the H_2 , CO, CO_2 , and CH_4 levels with typical accuracies of between 1 and 10% of full scale, automatically orchestrate the sample and calibration selection valves, and visually and electronically output the results.

Quick (1- to 2-min response time) measurements of O_2 and H_2O levels will be done by electrochemical and capacitance techniques, respectively, and the steady-state H_2O concentration will be measured by electrolytic trace moisture analyzers with an accuracy of 5% of full scale or better.

Writing the specifications for a complete analytical system was completed at year's end. The specifications will be reviewed by the project during January 1982.

The exact configuration of the impurities control system is at the moment indeterminate. We plan to vent the loop to maintain at its nominal value the concentration of the impurity that is produced by graphite oxidation in the greatest abundance. Makeup gas mixtures will then be added to bring up the other impurities to the test level concentrations. Systems for gas cleanup through a bypass regeneration loop were investigated, but one will not be installed for initial tests.

A digital simulation of the dynamics of the loop impurity levels is planned to help devise an effective control strategy.

Fig. 9.7. Automated gas chromatograph and valving for gaseous impurity measurement system in the Core Support Performance Test.

9.3.5 Design of Test Vessel and Internals — W. R. Clark

The CSPT test vessel and its internal components provide the desired helium temperature and flow paths for the graphite testing while accommodating requirements for pressure boundary integrity. This concept requires heating the incoming helium to supply a continuous flow at 1000°C (1832°F) to the graphite test specimen while moderating the temperatures to which the pressure boundary is exposed, so that the vessel material retains sufficient strength to contain the 8.3 MPa (1200 psi) design pressure.

As a drawing of the assembled test vessel and internal components (Fig. 9.8) illustrates, the helium to be heated and exposed to the test section enters the vessel through the upper end closure. The helium flows downward first in the tubing of a recuperative heat exchanger, where heat from the exiting helium is transferred to it. It then flows through an annular space to the lower end of the vessel, where it reverses direction and flows upward through the helium heater, which raises its temperature to that required for the test. The flow is then directed through an annular region surrounding the core support post assembly (the test piece), after which it passes over the outside of the recuperative heat exchanger and on to the vessel outlet. At this location it is mixed with the attemperation flow, which enters near the lower end of the vessel and then flows upward adjacent to the vessel shell through an annular space to the outlet. In the piping just downstream of the vessel, a commercial static flow mixer is installed. This mixer serves the dual function of eliminating hot spots in the flow exiting the vessel and providing a non-vital surface to collect graphite particles entrained as the helium flows past the test piece.

The test vessel shell is a 406-mm-OD (16-in.) cylinder, 5.44 m (214 in.) long. It is designed to withstand 8.3 MPa (1200 psi) internal pressure plus anticipated piping and mechanical loads at 570°C (1050°F). The end closures are Grayloc flange assemblies, both removable for access to the vessel internals. Flow inlet and outlet nozzles are Sweepolet forged fittings, as are the two access ports provided to permit inspection of the sample without requiring its removal from the vessel. These ports

Fig. 9.8. Test vessel assembly for the Core Support Performance Test.

are closed with 4-in. (nominal) Grayloc flange assemblies. The vessel is entirely type 304H stainless steel, except the Grayloc forgings, which are type 316 stainless steel. All material is certified to be acceptable for *ASME Boiler and Pressure Vessel Code* Section III, Class 1, applications, and the design and fabrication are being performed in accordance with these rules. The only materials not available from the CFTL program are the Grayloc assemblies for the access ports and a 3.66-m-long (12-ft) section of stainless steel pipe for the cylindrical shell of the test vessel. Sufficient Inconel 625 is available for this section; however, several factors led to a decision not to use this material. First, the in-service radiographic examination planned for the CFTL is no longer a requirement, so the thinner wall that could be achieved by using the Inconel 625 is no longer an advantage. Second, the increased operating temperature makes the Inconel only marginally usable. Finally, the addition of the access ports increases the number of dissimilar-metal welds in the test vessel pressure boundary and thus would increase the cost of fabrication with the Inconel. Thus, the decision for an entirely stainless steel vessel was made.

Drawings of the vessel shell have been approved and released, and the ORNL Plant and Equipment Division shops have begun the vessel fabrication. Some detailed stress analysis remains to be completed, but preliminary results and experience with the similar CFTL vessel indicate that the design should be acceptable.

The test vessel internal components are designed to provide as efficiently as possible the required environment for the graphite corrosion tests. For certain tests, the sample is to be exposed to helium at 1000°C (1832°F). However, the circulators cannot tolerate helium temperatures above 450°C (850°F). Thus, the helium must be heated and cooled between these temperatures during each circuit that it travels in the CFTL helium system. For the 0.45-kg/s (1.0-lb/s) test section flow rate, this would require a continuous heat input of about 1.36 MW. Over a six-month-long test, the cost of the required electrical power is high enough to make economical the inclusion of a recuperative heat exchanger (Fig. 9.9). The inlet flow passes through approximately 170 stainless steel tubes, each 9.5 mm (3/8 in.) in diameter and 1.83 m (6 ft) long, and recovers substantial heat from the leaving flow.

Fig. 9.9. Detail of upper end of test vessel for the Core Support Performance Test.

The remainder of the heat input is provided by the helium heater, which consists (Fig. 9.10) of six each 915-mm-long (36-in.) electric resistance heaters. Each heater is a heating element made of a 5.4-mm-diam (0.214-in.) HT molybdenum wire wrapped helically around the exterior of a grooved alumina core. Each of the six heaters is capable of an output of 0.25 MW, to yield a total helium heater capacity of 1.5 MW. Twelve commercial ceramic-insulated power feedthroughs penetrate the lower Grayloc flange closure. The leads to the heating element are planned to be a cluster of six identical HT molybdenum wires swaged inside a 22-mm (7/8-in.) molybdenum tube around the central wire, which is a continuation of the heating element. This concept was developed because of the difficulty in fabricating a brazed joint at the hot end of the heater and the inability to obtain HT molybdenum rods in a diameter large enough to maintain adequately low heat generation rates in the leads. Each heater is installed in an individual alumina cylinder to ensure adequate velocities for good heat transfer and to avoid arcing between heaters. The entire helium heater is supported from the lower Grayloc flange closure and can be inserted and removed independently of the remaining vessel internals. High-purity alumina (99.8% Al_2O_3) and HT molybdenum were selected as the materials for the hot region of the helium heater, both for their ability to perform in the extremely high-temperature environment and for their anticipated compatibility with the graphite test piece; that is, they are expected to release no impurities that would affect the reaction rate. Preliminary discussions with potential vendors indicate that the required materials can be obtained and that fabrication of the proposed design is feasible.

To simulate actual reactor conditions requires that the graphite test piece be placed under an 88-kN (20,000-lb) compressive load. A differential pressure applied to a bellows mounted from the top vessel closure will provide the necessary force, which is then transmitted through an alumina load post to the top end of the test piece. The test piece is restrained at its lower end by an alumina support plate.

At the end of 1981, design of the test vessel internals was still in progress. The extreme conditions to which the internal components will be exposed has led to a concentrated effort to determine that such components

Fig. 9.10. Detail of helium heater for the Core Support Performance Test.

are available and can perform as required. The requirements for high-quality materials in somewhat unusual product forms limit the available sources, but indications have been received that the components can be obtained.

9.3.6 Design of Loop Modifications — A. G. Grindell

All the design work for the CSPT had been initiated by the last quarter of calendar year 1981. Specifications for an additional 1 MW of power supply control equipment were prepared and approved for procurement. Design of the interconnections between an existing 0.5-MW power supply and the CSPT helium heater was initiated. Specifications for the impurity measurement system were prepared, and project review began at the year-end. The design of the test vessel was completed and approved, and the design of the helium heater and the internal flow passageways was nearly complete. The design of the piping modifications necessary to accommodate the installation of the test vessel was approximately 75% complete.

9.3.6.1 Electrical Design — T. L. Hudson

The conceptual design for the CSPT indicated that about 1.5 MW of power is required to heat the circulating helium to the required test temperatures. Although the installed capacity of the CFTL secondary transformers is 5 MW (two 2.5-MW transformers reduce the primary supply from 13,800 to 460 V), the capacity of the installed power control system is only 0.5 MW. Thus, two additional control systems, each having a capacity of 0.5 MW, are required. The control systems will provide a continuously adjustable voltage at 60 Hz, and each will consist of an induction voltage regulator supplying a multitap transformer. The new systems will be similar to the existing control system except that the silicon-controlled rectifiers for tap switching will not be installed. Each of the three systems will control power to two 250-kW helium heaters.

The primary power system for the CFTL consists of a 13.8-kV circuit; two 2500-kVA, one 1000-kVA, and one 750-kVA transformers; and the four 480-V switchgear systems, as shown in Fig. 9.11. These dry-type transformers, along with the switchgear, were installed during the construction of the Stage Alpha CFTL.

Figure 9.13 shows schematically each of the two additional control systems, including the control equipment and the associated protective and power measurement devices.

Detailed design has begun for the installation of wiring from the existing 0.5-MW power control system to the test loop.

Fig. 9.12. Schematic diagram of 0.5-MW power supply 1 for the Core Support Performance Test.

Fig. 9.13. Schematic diagram for 0.5-MW power supplies 2 and 3 for the Core Support Performance Test.

9.3.6.2 Piping — W. R. Clark

At the end of calendar year 1981 detailed design of the CSPT primary helium piping was about 75% complete. The rapid progress of the design was largely attributable to the intact use of much of the piping installed for the Stage Alpha CFTL and to providing for nearly all the changes needed for the CSPT by reclaiming and reusing the remainder of the Stage Alpha piping.

The proposed CSPT piping layout is shown in Fig. 9.14. Three circulators, connected in series, provide the head to drive the helium through the loop; the flow passes through a flowmeter just downstream of the circulator, where it is divided into two segments. The first, the test flow, will be heated before flowing past the test piece; the other, the attemperation flow, will flow through an annular space adjacent to the vessel shell and moderate its temperature. At the test vessel outlet the flows are recombined and carried to the helium-to-air heat exchanger, where the helium temperature is reduced to a maximum of 450°C (850°F) to avoid damage to the circulators. The flow then passes through a full-flow filter before returning to the circulator inlet.

The components that were installed for the CFTL Stage Alpha and are usable without modification for the CSPT include the circulators, two vortex shedding flowmeters, the heat exchanger, the filter, and a substantial amount of the interconnecting primary piping. Additionally, the pressure relief system and evacuation system can be used as they are. However, some piping changes and additions will be necessary. Installation of the test vessel requires rerouting the main inlet piping as well as the piping from the vessel outlet to the heat exchanger. A 4-in. (nominal) Grayloc flange assembly in the inlet piping permits disassembly of the top end closure of the test vessel from the inlet piping and avoids the rewelding associated with pipe cutting. Thus, the vessel closure may be removed and replaced rather conveniently to provide access to the vessel internals and the test piece. The attemperation flow piping, which includes an existing vortex shedding flowmeter, will be added. Two previously purchased bellows-sealed control valves will be installed to divide the helium flow appropriately. Finally, some structural steel and pipe hanger modifications will be made to support the test vessel and new piping.

Fig. 9.14. Primary helium piping system for the Core Support Performance Test.

The design pressure of the CFTL piping is 11.7 MPa (1700 psi) at 480°C (900°F). However, because of the higher temperature of the helium exiting the test vessel, this particular section of piping to the heat exchanger inlet is designed for 600°C (1100°F). Stresses due to thermal

expansion and weight loading have been analyzed with the MEC-21 computer code. Preliminary indications are that the planned layout does not produce excessive stresses, and final computations are being made. Results of the analysis have been used to select appropriate locations for pipe hangers. Many portions of the analyses done for Stage Alpha CFTL in such areas as overpressure protection, dynamic reaction of the piping during venting, and loop pressure drop are applicable to CSPT; areas that need updating will be addressed in early 1982.

The current schedule calls for the design to be completed in time to initiate removal and reclamation of the Stage Alpha piping in March 1982. Piping subassemblies will be fabricated at the shops of the ORNL Plant and Equipment Division to minimize the number of field welds required. Final on-site installation will be performed by Y-12 craftsmen.

9.3.6.3 Building — W. R. Clark

Very few additional building modifications are anticipated to be required for the CSPT. Some work begun during CFTL construction was completed recently. The work on the loop enclosure was concluded with the installation of a roll-up door in the west wall. This door provides essentially a floor-to-ceiling, column-to-column opening about 4.3 m wide by 7.6 m high (14 by 25 ft) for equipment access. Because the door will not withstand the loop enclosure overpressure of 1.44 kPa (30 psf) that the blowout panel in the east wall was designed for, modifications were made to reduce the blowout panel rating to 960 Pa (20 psf) in accordance with the manufacturer's instructions.

Freezing and subsequent bursting of water-filled fire protection sprinkler system piping during cold winter weather has renewed concerns about possible equipment damage due to inadvertent water releases. Possible remedies and safeguards will be examined in early 1982.

9.3.6.4 Instrumentation and Controls — P. G. Herndon

The conceptual design of additions and revisions to the instrumentation and control systems necessary to adapt the Stage Alpha CFTL to the requirements of the CSPT was completed.

Two 4-in. (nominal) helium flow control valves purchased for the GCFR test program will be installed in the loop. Pneumatically powered actuators must be purchased and installed on the existing valve bodies. A hydraulic actuator system was purchased with these valves, but it is far too complex for CSPT requirements and would be much more costly to install and operate than the pneumatic type.

With the exception of the helium flow control valve actuators, both the flow and the test piece heater power control systems can be configured with existing components. The present heat exchanger control system can handle all CSPT requirements. However, additions to the protective interlock system will require an increase in the capacity of the programmable controller. The necessary components can be procured and added to the existing system at moderate expense.

Instrumentation for the initial test piece poses no special problem. The high-quality stainless-steel-sheathed thermocouples required in the annular flow spaces and in the graphite test piece are on hand. The possible use of commercially available, high-temperature strain gage elements [$<425^{\circ}\text{C}$ (800°F)] to measure the load applied to the test piece is under investigation. After evaluating several methods for applying a controlled load to the test piece, one that uses an internally pressurized bellows was selected. A precision pressure control system will be required to protect against bellows overpressure.

The cost of design, procurement, and installation was estimated for the conceptual instrumentation and control systems briefly described above. The major cost is associated with the procurement and installation of the gas analysis system and the impurities control system described in Sect. 9.3.4.

9.3.7 Analysis — N. C. Chen

9.3.7.1 Temperature in Test Vessel

The proposed test vessel cross section (Fig. 9.15) consists of three annular flow channels separated by walls that are insulated by corrugated layers of metal sheet containing trapped stagnant helium cells. These channels contain alternating helium flows: a hot test helium flow upward

NOTATIONS

- LAYERS ① ⑦ AND ⑫ = HELIUM CHANNELS 1, 2, AND 3
 LAYERS ② ⑥ ⑧ ⑪ AND ⑬ = CONVECTIVE HELIUM FILMS
 LAYER ③ = 12.7-mm (0.5-in.) THICK GRAPHITE SLEEVE
 LAYER ④ = 12.7-mm (0.5-in.) THICK INSULATOR
 LAYERS ⑤ AND ⑩ = 3.2-mm (0.125-in.) THICK SS
 LAYER ⑨ = 6.4-mm (0.25-in.) THICK INSULATOR
 LAYER ⑭ = 22.2-mm (0.875-in.) THICK SS VESSEL WALL
 LAYER ⑮ = 50.8-mm (2.0-in.) THICK KAOWOOL BLANKET
 LAYER ⑯ = CONVECTIVE AIR FILM
 LAYER ⑰ = AMBIENT AIR
 Q = HEAT TRANSFERED BY CONVECTION
 q = HEAT CONDUCTED THROUGH COMPOSITE WALL
 R = TOTAL RESISTANCE OF COMPOSITE WALL
 T = HELIUM AND AIR TEMPERATURES

Fig. 9.15. Schematic of test vessel cross section for the Core Support Performance Test. The wide arrows indicate the helium flow direction. All radial distances are measured from the centerline of the test vessel.

in the inner channel, surrounded by a preheated helium flow downward in the middle, and then upward flow of an attemperation helium stream in the outer channel. The inner composite wall consists of 13-mm (1/2-in.) graphite, 13-mm (1/2-in.) corrugated insulator, and 3.2-mm (1/8-in.) stainless steel. The outer wall consists of a 22-mm (7/8-in.) stainless steel wall and a 50-mm (2-in.) Kaowool blanket. The specifications of the channels and insulation layers in the test section are also shown in Fig. 9.15.

From the test section configuration, helium mass flows, and their inlet temperatures, we calculated the outlet temperatures, heat losses through composite walls, and the temperature distribution in the test section.

To simplify calculations, we assumed that a steady-state condition prevails such that the graphite sample and test helium remain in thermal equilibrium during the test. By considering our energy balance through channels with a multicomponent wall, three equations containing three outlet temperatures as unknowns were established. This set of simultaneous equations was then solved subject to the input parameters listed in Table 9.3. Once the outlet temperatures were obtained, various heat fluxes were calculated. The solutions are presented in Figs. 9.16 and 9.17.

Table 9.3. Input parameters for temperature and heat loss calculations of CSPT test section

Flow rate, kg/s (lb/s)	
Test helium, w_1	0.45 (1.0)
Preheated helium, w_2	0.45 (1.0)
Attemperated helium, w_3	0.33 (0.73)
Inlet temperature, °C (°F)	
Test helium, T_{10}	1000 (1832)
Preheated helium, T_{20}	750 (1382)
Attemperated helium, T_{30}	450 (842)
Film coefficient, $W/(m^2 \cdot K)$ [Btu/(h·ft ² ·°F)]	
Helium in channel 1, h_1	398 (70)
Helium in channel 2, h_2	767 (135)
Helium in channel 3, h_4	505 (89)
Air, h_6	14 (2.5)

Fig. 9.16. Schematic temperature distribution of heat flux in the Core Support Performance Test.

Figure 9.16 represents the calculated temperatures and heat flux in each channel. The temperature profile shows a difference of about 22°C (40°F) in the helium flowing past the test piece. Also note that the temperature of the preheated helium drops about 18°C (32°F). Finally, the temperature in the attemperation helium rises about 50°C (90°F) over the test section. This increase over the inlet temperature may require more attemperated helium mass flow for mixing if a predetermined mixing temperature is desirable. For the heat flux, a significant amount of heat ($q_2 \approx 0.1 \text{ MW}$) is transmitted to the outer annulus, in which the attemperation helium flows. This amount of heat represents 7.7% of the total power supplied (1.3 MW).

Fig. 9.17. Temperature distribution in the test section of the Core Support Performance Test.

Figure 9.17 shows the calculated temperature profile layer by layer, as obtained from analyses of heat conduction paths for the test section covering a temperature range from 990 to 21°C (1812 to 70°F). From the gas in channel 1, which is the entering helium at 990°C, the temperature drops to 741°C (1366°F), which is the mean temperature of channel 2, over the first composite wall. The temperature continues to drop to 475°C (887°F), which is the mean temperature of channel 3, through the second composite wall. Finally, the temperature decreases from 475 to 21°C, the assumed room air temperature, over the last composite wall. Note that the mean temperature of the test vessel wall is 464°C (867°F), which is below the maximum allowable temperature for the stainless steel.

In summary, the important conclusions follow:

1. A significant amount of heat (about 0.1 MW or 7.7% of the total power supply of 1.3 MW) is transferred to the attemperation helium flowing along the vessel wall.

2. With a 50-mm (2-in.) layer of Kaowool as external insulation, the calculated vessel wall temperature [464°C (867°F)] is within the permissible operating range.

3. Because of heat loss to the outer annuli, the temperature of the helium decreases by about 22°C (40°F) as it flows past the test piece.

4. Heat loss also decreases the temperature of the helium leaving the recuperative heat exchanger by 18°C (32°F) before it enters the heater section.

5. The temperature of the attemperation helium increases by 50°C (90°F) by heat transferred from the inner annuli.

9.3.7.2 Temperature in Test Piece

A determination of the temperatures in the graphite test piece during thermal transients will be required both for defining the instrumentation and for establishing the conditions during the test. No computational procedure exists that will couple the effects of the conductive transport of heat in the test piece with the convective transport of heat in the helium.

Analysis has begun to determine the time required to bring the test piece to the gas temperature as the test sequence begins. The problem has been represented analytically as a boundary value problem with temperature varying spatially and temporally. Both analytical and numerical techniques are being exercised to determine an appropriate solution.

We have developed an analytical approach that is limited to simple two-dimensional cylindrical geometry. Even for this geometric representation, the evaluation of the infinite series of complicated orthogonal functions that results from the analytical solution is not tractable. As a consequence, we have sought the development of a numerical algorithm to solve the problem.

This algorithm is based on an integrated compartment method (ICM) that combines the merits of finite-difference methods and finite-element methods. The ICM has been successfully applied in the solution of problems in biology and air pollution transport phenomena. However, the validity of this method for the solution of heat transport problems has not been demonstrated.

The problem formation has involved a number of assumptions, which include that (1) helium is incompressible, (2) helium thermophysical properties remain constant during transient, and (3) interactions between velocity and temperature fields are only partially accounted for. The last assumption is referred to the decoupling of the velocity and temperature. In the calculations, the velocity field at each compartment is given as input derived from the continuity equation.

Two cases were studied: one with the ideal body, the other with actual geometry. In the first case, a simple cylindrical body of the graphite test sample is assumed to be submerged in a stream of hot helium without any supporting structures. Test cases indicate that the graphite body is essentially at the helium temperature about 24 min after the gas has reached its steady-state maximum temperature.

The code has been refined to accommodate the actual geometry including the alumina sleeve and end piece. This refinement includes the following important modifications: (1) a significant increase in compartment numbers, (2) a provision to handle a multimaterial structure, and (3) an automation to calculate velocity field in each compartment. The analysis indicated that temperature equilibrium would be attained in about 3 h. The low thermal conductivity of the alumina end piece greatly lengthens the time to reach thermal equilibrium.

9.3.7.3 Pressure Loss in Test Vessel

The CSPT test vessel consists of three major components: recuperative heat exchanger, test section, and heater assembly. They are enclosed in the vertical test vessel with the exchanger at the top, test section in the middle, and heater unit at the bottom. The objective of this configuration is to raise helium to the required temperature at the test section through a two-stage heating process while reducing heater power supply.

The two-stage heating — first by the exchanger, second by the heater — requires helium to be routed in an alternating flow through channels separated by composite walls, as described in Sect. 9.7.3.1. To begin, the primary helium starts from the circulator and enters the recuperative heat exchanger. On being heated and leaving the exchanger, the preheated helium flows downward through the channel and enters the lower plenum of

the heater unit. Then the preheated helium is further heated through six annular heaters and reaches the required temperature at the upper plenum of the heater. This upflow hot helium continues past the test section, where a graphite sample is positioned, and then the gas reenters the recuperative heat exchanger. In the exchanger the helium cools and is ready for mixing with the attemperation helium. The mixed stream goes through an air-cooled heat exchanger and then returns to the circulator. This completes the helium looping system.

For this helium path, local pressure drop can be calculated component by component. By summing over all contributions, the total pressure loss in the test vessel could be obtained. The breakdown of the pressure drop by subcomponents is listed in Table 9.4 without giving calculational details.

Table 9.4. Pressure loss in the test vessel

Component	Source	Pressure loss	
		(kPa)	(psi)
Recuperative heat exchanger	Tube side	2.6	0.37
	Shell side	0.4	0.06
	Exit and entrance	<u>12.7</u>	<u>1.84</u>
	Subtotal	15.7	2.27
Test section	Annular window	0.09	0.013
Heater unit	U-turn	1.0	0.15
	Sudden contraction	3.0	0.44
	Heater	<u>13.4</u>	<u>1.94</u>
	Subtotal	17.4	2.53
Overall		33.2	4.81

9.3.7.4 Performance of Recuperative Heat Exchanger

With objectives of reducing both heater power supply and attemper-
ation helium in mixing, a recuperative heat exchanger was designed specifi-
cally for the CSPT. The recuperative heat exchanger is a device that

transfers the heat from the hot exit helium to the cold inlet helium. Without the exchanger, the energy required to heat the incoming helium from the circulator to the desired high temperature at the test section would have been provided entirely by the helium heater. With the installation of the recuperative heat exchanger, the heater power requirement can be reduced substantially, depending on the heat exchanger efficiency.

Subject to the high-pressure and high-temperature test conditions plus the geometric constraints of the test vessel, the currently proposed recuperative heat exchanger is a tube-and-shell type in a counterflow configuration. The size of the unit is 330 mm (13 in.) in diameter by 1.83 m (6 ft) in height, with each end plenum serving as a tubesheet. Downflow inlet helium in the tube side extracts heat from the upflow helium in the shell side. The tube-side inlet temperature is at 450°C, and the shell-side inlet temperature is maintained at 1000°C; helium mass flow is 0.45 kg/s (1.0 lb/s).

Analysis relating the maximum amount of heat transferred by the unit to the tube length as a function of total tube numbers is illustrated in Fig. 9.18. The temperature indicated on the ordinate with the heat transmission q represents the preheated helium outlet temperature. The calculations were based on a common shell inner diameter of 260 mm (10 1/4 in.), an outer diameter of 330 mm (13 in.), and a fixed tube outside diameter of 19 mm (3/8 in.), plus constant helium thermal properties.

As shown in Fig. 9.18, for a given tube number n , q increased with length slightly nonlinearly. For a constant heat load, doubling the tube number roughly reduced the tube length by half. The proposed optimal design point indicated a q of about 0.75 MW, n of 152, and l of 1.83 m (6 ft). At this design point, the energy supply by the recuperative heat exchanger and the heater assembly will be about equal. Consequently, a significant reduction in power in the heater is achieved; however, this saving is obtained at the expense of the heat exchanger cost.

Fig. 9.18. Performance characteristics of the recuperative heat exchanger for the Core Support Performance Test.

9.3.7.5 Performance of Helium Heater

A major objective of the CSPT is to study accelerated corrosion of the core graphite support structure in helium at reactor conditions. To achieve the high temperature in the test section, a helium heater must be designed.

The heater design consists of six heater units, a central one surrounded by five equally spaced units. Each heater unit contains a central mandrel on which a 5.44-mm-diam (0.214-in.) molybdenum heater wire is wrapped and half buried in the preformed grooves. The heater assembly is placed inside an alumina tube to form an annular flow passage with a 3.2-mm (1/8-in.) radial spacing. The maximum power of the heater bundle is 1.3 MW, with the heated length of about 0.9 m (3 ft) at a pitch of 6.35 mm (0.25 in.). The total helium flow is 0.45 kg/s (1.0 lb/s) with a helium inlet temperature of 450°C (842°F) and pressure of 7.24 MPa (1050 psi).

A mathematical model and a computer code have been developed to evaluate thermal flow conditions of the annular flow helium heater design. The model accounts for the effects of convection and radiation heat transport and the temperature-dependent resistivity of the molybdenum heating wire. The assumptions are steady-state, incompressible, and one-dimension flow, plus a negligible temperature gradient across the wire.

For the test conditions mentioned above, the calculations showed a maximum heat transfer coefficient of $2780 \text{ W/(m}^2\cdot\text{K)}$ ($490 \text{ Btu/h}\cdot\text{ft}^2\cdot^\circ\text{F}$) and an overall pressure drop of 17.4 kPa (2.53 psi) for the proposed heater design.

Radiation is less important at the cold (inlet) end but is most significant at the hot (outlet) end. For the case with full power and nonuniform heating (variable molybdenum electrical resistivity), the maximum radiation transport was found to be about 14% of the total heat transferred.

Including the variable resistivity and heat transport by convection and radiation, calculation showed that the maximum molybdenum temperature of 1400°C occurred at the outlet, which stays below the maximum permissible limit of 1600°C . Consequently, the operating temperature range for the helium heater design has a safe margin. More detailed calculations are available.¹⁰

9.3.8 Core Support Performance Test Procurement Activities — A. G. Grindell

Several procurement activities were initiated during the last quarter of 1981. Purchase orders were placed for the power control equipment in the 1-MW addition for the CSPT heater and for the stainless steel pipe needed to fabricate the middle course of the test vessel; these orders are for long-time delivery material and equipment. A request for funds prepared for the impurities measurement system was approved. Initial contacts were made to obtain technical information about the molybdenum and alumina components needed for the helium heater and about other alumina components for the test vessel internals.

9.3.8.1 Procurement of Electrical Power Control System — T. L. Hudson

Procurement of the two additional 0.5-MW power control systems for the CSPT helium heater was initiated in October, and purchase orders were placed in November. Two induction voltage regulators are being purchased according to an existing specification. A specification for the special multitap transformers was prepared; it preserves sufficient space to install the silicon-controlled rectifier switches later.

We have reviewed and approved the drawings for the two induction voltage regulators, and the approved drawings have been issued by General Electric Company of Pittsfield, Massachusetts, the manufacturer. Delivery is scheduled for May 1, 1982.

We have reviewed and approved the drawings for the two transformers, and the approved drawings have been issued by Robicon Company of Pittsburgh, the manufacturer. Delivery is scheduled for May 1, 1982, also.

9.3.8.2 Test Vessel and Piping Procurement — W. R. Clark and H. C. Young

A concentrated effort has been made to design the CSPT piping and test vessel to utilize the piping, fittings, and other material left over from the construction of the Stage Alpha CFTL. Likewise, an effort to reclaim and reuse as much as practical of the piping fabricated and installed during the Stage Alpha construction was made. As a result, we will need to purchase only very little additional pressure boundary material for the CSPT.

For the test vessel shell, in particular, existing Grayloc hub and clamp sets are being used for the end closures. Previously purchased Sweepolet nozzles are utilized for flow inlet and outlet, as well as for penetrations to provide access to examine the graphite specimen while in place in the vessel. Part of the shell is to be rolled from available stainless steel plate. However, a 3.66-m (12-ft) length of 406-mm (16-in.) pipe made of type 304H stainless steel has been ordered to complete fabrication of the test vessel. This pipe section, like the rest of the test vessel pressure boundary, is certified *ASME Boiler and Pressure Vessel Code* Section III, Class 1 Material. Sufficient Inconel 625 plate was available to form this section of the shell, but,

because of the higher planned operating temperatures, the elimination of a previous in-service radiography requirement, and the several dissimilar-metal welds that would be required should the Inconel 625 be used, the decision was made to purchase additional stainless steel to complete the vessel. An order for this pipe was awarded in October 1981, with delivery promised in February 1982.

Closures for the test vessel access ports must be suitable to withstand loop operating pressures and temperatures, and it is desirable that they be easy to assemble and disassemble as well. For these reasons Grayloc hub and clamp sets were selected. A substantial delivery time was indicated by the vendor, so, to keep the vessel fabrication on schedule, spare Graylocs purchased for the Blowdown Heat Transfer Project were borrowed for use by the CSPT. A purchase order has been written to buy replacements for the borrowed parts.

An additional Grayloc assembly is required to facilitate disassembly of the main test section flow inlet piping and permit removal of the top vessel closure. Once this is removed, the loading device assembly is accessible and can be removed, exposing the graphite test piece for examination or replacement. Items needed for this Grayloc assembly were included in the previously mentioned requisition. The bulk of the piping can be fabricated and installed before the receipt of these parts, so their early delivery is not crucial to the schedule.

No purchase orders have been issued for any test vessel internal components, but preliminary discussions have been held with potential vendors regarding material selection and ability to fabricate planned components. As noted in Sect. 9.3.5, the test temperatures require the use of somewhat exotic materials, in particular alumina and HT molybdenum for the helium heater and alumina for other vessel internals. However, suppliers have indicated their willingness and ability to provide the materials. Manufacturers also have been approached regarding the fabrication of the helium heater and have stated that manufacturing and assembling the helical heating element of HT molybdenum and the alumina core are within their capabilities. Other required internal components, such as the loading bellows and pressure-boundary feedthroughs for helium heater power, also seem to be available.

9.3.8.3 Procurement of Impurity Measurement and Control Systems —
P. G. Herndon

The specifications for the impurity measurement system were written and, at year-end, were being subjected to project review. A request for funds for the purchase of the system was prepared and approved. We expect to complete the specification and to initiate procurement of this long-delivery equipment early in 1982.

9.3.9 Test of Design Features — A. G. Grindell

9.3.9.1 Compatibility of HT Molybdenum with Al_2O_3 and Carbon —
J. H. DeVan

The heating elements currently envisaged for the helium heater in the CSPT are made of relatively pure HT molybdenum wire or rod wrapped helically on Al_2O_3 cylindrical cores and supported in a vertical orientation. In qualifying this helium heater concept for service in the CSPT it is important to demonstrate that reactions of the type

will not degrade the mechanical properties of the molybdenum. Accordingly, compatibility tests are now under way to determine experimentally the extent of interaction between HT molybdenum and the Coors high-density alumina (for heating element support) at the highest contact temperature anticipated for the assembly (1350°C). The test specimens consist of 6.4-mm-OD (1/4-in.) HT molybdenum rods placed in tight-fitting 6.4-mm semicircular grooves in Al_2O_3 blocks. The contact force is the weight of the molybdenum rod, which rests directly on the ceramic. Two identical specimen assemblies are being exposed at 1350°C in a tungsten-mesh, resistively heated furnace in a dynamic vacuum of 0.13 mPa (10^{-6} torr).

Contained in the same vacuum furnace are couples of HT molybdenum contacting Stackpole 2020 graphite and Coors Al_2O_3 contacting Stackpole 2020 graphite. The purpose of the former couples is to determine the kinetics of the carburization of molybdenum in contact with "pure" carbon at 1350°C . These data will provide a conservative estimate of the reduction of heater element cross section that may accrue from molybdenum

carbide formation through reactions with carbon-containing impurities in the CSPT helium. The Al_2O_3 -graphite couples are intended to test reactions of the type $2\text{Al}_2\text{O}_3 + 9\text{C} \rightarrow \text{Al}_4\text{C}_3 + 6\text{CO}$ or $\text{Al}_2\text{O}_3 + \text{C} \rightarrow 2\text{AlO} + \text{CO}$. These reactions are of interest in determining the suitability of Al_2O_3 as the interfacing material between the CSPT graphite test piece and the bellows-actuated loading system.

All couples are being exposed for six months at 1350°C with periodic examinations of the materials.

9.3.9.2 Compatibility of Molybdenum with the Impure Gas and Graphite — H. Inouye

The objective of this task is to determine if molybdenum wires (5.44 mm diam) can be used to heat the CSPT helium to 1000°C in the presence of low levels of H_2 , H_2O , CO , and CO_2 impurities. Although molybdenum corrosion in the environment is of interest, the primary concern is the potential vapor transport of molybdenum oxide to the downstream graphite test piece and the influence of this molybdenum contamination on the graphite oxidation kinetics.

For the proposed test gas with an $\text{H}_2:\text{H}_2\text{O}$ ratio of 5, MoO_2 formation is expected below about 627°C , and, for a $\text{CO}:\text{CO}_2$ ratio of 2, oxidizing conditions exist at all temperatures. The gas conditions do not favor the formation of MoO_3 . However, when all four gas species coexist at high temperatures, the water-gas shift reaction $\text{CO} + \text{H}_2\text{O} \rightarrow \text{CO}_2 + \text{H}_2$ shifts the gas ratios to lower $\text{H}_2:\text{H}_2\text{O}$ and higher $\text{CO}:\text{CO}_2$ ratios, so that at equilibrium oxidation of molybdenum to MoO_2 is expected below 707°C (1304°C).

Thus, the test plan is to determine whether the oxidation of graphite is influenced by upstream molybdenum test specimens and verify whether the water-gas shift affects molybdenum oxidation as predicted by the thermodynamic calculations. To do this, graphite rod specimens 6.35 mm diam by 25.4 mm long will be exposed to recirculating helium at 34 kPa gage (5 psig) and 12 L/min flow containing 100 H_2 , 100 CO , 50 CO_2 , 20 H_2O ppm based on 7.2 MPa (71 atm) helium pressure (7100 H_2 , 7100 CO , 3550 CO_2 , 1420 H_2O μatm) at 700°C . Molybdenum specimens 6.35 mm diam by 51 mm long will be positioned upstream of the graphite rods at the calculated molybdenum heater temperatures (600 – 900°C). A control run under the same test

conditions without the molybdenum specimens will be made to determine the difference, if any, in the oxidation kinetics of the graphite. A second set of tests at a graphite temperature of 1000°C, higher molybdenum specimen temperatures, and possibly higher impurity levels is also planned.

To date two existing test loops have been modified, the gas analysis techniques and instrument calibration methods have been established, and the initial graphite-molybdenum test at a graphite temperature of 700°C has logged 250 h of a scheduled 1000-h run. Analysis of the test gas of the retort inlet and outlet shows small differences in the H₂, CO, and CO₂ levels; however, the exiting H₂O level is 50 to 60% lower than the entering concentration.

9.4 COMPONENT FLOW TEST LOOP SHAKEDOWN OPERATIONS — A. G. Grindell

The shakedown of the Stage Alpha CFTL was completed successfully during December 1981. There were two shakedown campaigns: the first ran from the end of September to the first of November 1981, and the second occupied the week of December 7 to December 11, 1981. During the first campaign most of the electrical and instrumentation and control systems were exercised satisfactorily. However, the circulator operation was characterized by apparent instabilities in both the thrust and journal bearings, which led to the second campaign. Between campaigns the journals and pivoted pad bearings for circulator 1 were refurbished at Mechanical Technology Inc. (MTI), the circulator manufacturer. During the second campaign all three circulators, including the refurbished No. 1, were operated satisfactorily up to the maximum speed of 23,500 rpm, and the remaining electrical and instrumentation and control systems were operated satisfactorily. The shakedown of the circulators indicated a need to upgrade the cables that carry film thickness signals from the circulator bearing sensors to the control room readout instruments.

9.4.1 Circulator Operation — H. C. Young

The rotary assemblies for the three helium circulators for the CFTL were assembled at the CFTL site in June 1981,¹¹ operated satisfactorily in air to 23,500 rpm, and then inserted into their respective pressure vessels

with the assistance of a field representative from MTI, the circulator manufacturer. (In this report a circulator rotary assembly will be called a circulator and a pressure vessel will be referred to by that name.)

A circulator, mounted on its base-assembly fixture, is shown in Fig. 9.19 ready for operation in air before installation into a pressure vessel. The circulators and pressure vessels were numbered 1, 2, and 3 during manufacture. The three circulators and pressure vessels are interchangeable. Figure 9.20 shows all three circulators (1, 2, and 3) installed in respective pressure vessels (1, 2, and 3).

The CFTL primary piping and loop components were maintained under an argon environment to protect the circulator chrome-oxide bearing surfaces against the effects of moisture from just after their insertion into the pressure vessels on June 19, 1981, until the CFTL was evacuated and pressurized to 4.83 MPa (700 psig) helium on September 28, 1981.

PHOTO 7585-81

Fig. 9.19. Helium circulator mounted on assembly fixture before installation into the pressure vessel of the Component Flow Test Loop.

Fig. 9.20. Three helium circulators installed in their respective pressure vessels in the Component Flow Test Loop.

All three circulators were initially operated in series on September 29, 1981. Head-flow performance data agreed well with the test data obtained at MTI for each individual circulator. The developed head for a single circulator at a test speed and flow condition was multiplied by 3 and compared with the test head data for the three circulators at test speed and flow.

Initial bearing operation, as monitored by the bearing displacement probe readouts on oscilloscopes, appeared to be less stable than the bearings operation observed at MTI. The thrust bearings operated at nearly double the expected clearance, and the fan end journal bearing on circulator 1 appeared to be unstable at about 14,000 rpm. During this first shakedown campaign, circulator 1 experienced a bearing rub, and it was necessary to refurbish the two journal bearings and the associated pivoted bearing pads at MTI.

Early during the second and final campaign in late November and early December 1981, circulators 2 and 3 were operated in helium with pressure vessel 1 empty. Circulator 1 had been transported to MTI for the bearing refurbishment. Later circulator 2 was installed into pressure vessel 1, and refurbished circulator 1 was installed in pressure vessel 2. The shakedown was completed with that configuration, which still obtains at the beginning of 1982.

Shakedown operation of the circulators was then successfully completed, and a 4-h continuous test run was made at 23,500 rpm at a final pressure and temperature of 6.0 MPa (870 psia) and 350°C (650°F) on December 11, 1981, some 2.5 months after initial operation in the CFTL.

9.4.2 Circulator Head-Flow Performance — H. C. Young

The test bundle and loop control valves, originally planned for the CFTL, were not installed for the CFTL Stage Alpha. The pressure drop calculated for the test bundle was simulated by a fixed orifice in the loop piping. Operation of all three circulators simultaneously in the Stage Alpha can be made on one constant-flow resistance line only and at speeds from 3000 to 23,500 rpm. The circulators were operated in various series combinations: all three simultaneously and all combinations of two circulators operating with the third circulator stopped; such operation increases flow resistance substantially. Also circulators 2 and 3 were operated with pressure vessel 1 empty; in that mode the flow resistance is slightly higher than for all three circulators operating simultaneously. Operation with only one circulator was avoided, because the flow resistance of two stopped circulators when added to the flow resistance of the loop piping would result in operation in the surge region.

The circulator-developed head was measured with a differential pressure instrument connected at the inlet of circulator 1 and the discharge of circulator 3. The head developed by each operating circulator was deduced from the MTI performance curves for a single circulator. Figure 9.21 shows typical head-flow curves for a single circulator from test data obtained during performance tests at MTI. Figure 9.22 shows circulator performance data measured with three circulators in operation

Fig. 9.21. Head-flow performance for a single helium circulator of the Component Flow Test Loop. Source of data: Mechanical Technology, Inc.

Fig. 9.22. Head-flow performance for three helium circulators operating in series in the Component Flow Test Loop.

in the CFTL. Figure 9.23 shows circulator performance data measured with two circulators in operation, with one circulator stopped. The head loss through the stopped circulator is noted.

Because the journal bearing instability had been noted only in circulator 1, and then only when it was installed in pressure vessel 1 and operated with helium, poor distribution of helium flow from the loop into

ORNL-DWG 82-9736

Fig. 9.23. Head-flow performance for two helium circulators operating in series with the third circulator stopped in the Component Flow Test Loop.

the inlet to pressure vessel 1 might be the cause of the bearing instability. After circulator 1 had been transported to MTI for bearing refurbishment, circulators 2 and 3 were operated in helium in their respective pressure vessels to test this hypothesis. Satisfactory operation at up to 23,500 rpm indicated that the bearing instability with circulator 1 may have had other roots. Figure 9.24 presents the performance data taken during this operation.

Fig. 9.24. Head-flow performance for two helium circulators operating in series with the third circulator pressure vessel empty.

9.4.3 Circulator Bearing Performance — H. C. Young

Initial operation in the CFTL with all three circulators operating was limited to about 19,000 rpm, because the thrust bearing clearance on circulator 3 appeared to be unstable when the speed was further increased. When only two circulators were operated, the additional flow resistance of the stopped circulator reduced the circulator flow, increased the thrust bearing load, and permitted stable bearing operation to the full speed, 23,500 rpm.

Referring to Fig. 9.25, the circulator design incorporates a one-way thrust bearing, and the rotor axis is slanted 8° from horizontal so that the thrust bearing is always loaded by gravity. The shaft end play is 0.10 mm (0.004 in.), and a bumper is provided to protect against complete thrust reversal.

Venting of a balance chamber on the back of the shrouder impeller controls the dynamic thrust force. At higher than circulator design flow, the thrust force is reduced, and Fig. 9.22 indicates that flow with the constant resistance system in the CFTL was higher than the design value.

The apparent thrust bearing instability was eventually attributed to noise on the bearing readout system. The MTI representative and MTI staff advised that the approximate 50- μ m (0.002-in.) thrust clearance, although double the <25 μ m measured during their single circulator tests, is acceptable. At higher loop pressures and/or lower flows the thrust bearing loading would increase and reduce the thrust bearing clearance. Thrust bearing operation in air, where the most unfavorable thrust conditions occur, was satisfactory.

While investigating the apparent thrust bearing problem discussed above, we noted that the oscilloscope trace of the fan-end journal bearing for circulator 1 was very erratic around 14,000 rpm. We described the symptoms to MTI by phone, and they initially attributed the phenomenon to electrical noise on the bearing capacitance probe readout system. Some of the problem definitely was attributable to noise, but the readout for this bearing differed so greatly from those observed during tests at MTI that we were reluctant to operate at higher speeds. Noise filters were added, but the readout of this one journal bearing was still very erratic.

Fig. 9.25. Cross section of a helium circulator installed in the pressure vessel.

With an MTI field representative observing the bearing readouts, all three circulators were operated, then all combinations of two circulators were operated at three different loop pressures, and the erratic bearing indication continued to appear around 14,000 rpm.

After consultation with the MTI staff, the MTI field representative removed circulator 1. The journal bearing clearances were closer to 50 μm (0.002 in.) rather than the desired 25 μm (0.001 in.). The clearances were then adjusted to the correct values, but the circulator would not start.

The circulator was totally disassembled, and the appearance of all bearing surfaces was good, although there was some molybdenum dioxide or chrome-oxide powder around the adjustment threads on the bearing pivots.

The journals and all six bearing pads were cleaned, sprayed with Molykote 321R, and baked overnight at 150°C (300°F) to drive off volatiles. [It was subsequently determined that the furnace actually controlled at 260°C (500°F).]

The circulator was reassembled and operated in air on November 1, 1981, to 23,500 rpm. The speed was reduced to 4000 rpm, increased to 19,000 rpm, and again reduced to 4000 rpm. After a few seconds at 4000 rpm, the impeller end journal bearing rubbed, as indicated on the oscilloscope; the stop button was pushed immediately.

The circulator was disassembled again. Both ends of the impeller end journal showed heavily burnished marks in the chrome-oxide surface; the mating location on all three bearing pads showed similar heavy burnish marks.

The shaft and bearings were returned to MTI for assessment of the damage. The bearing pads had been fabricated from 52100 bearing steel and had been heat-treated to Rockwell RC 60, with a final soak for 1 h at 260°C (500°F).

The inadvertent bakeout of the Molykote at 260°C (500°F), rather than at the desired 150°C (300°F), caused the pads to warp in two directions. The inside diameter increased by 0.10 mm (0.004 in.) and the inner surface was barrel shaped. When the circulator was run in air, this distortion significantly raised the lift-off speed, apparently to above 4000 rpm, resulting in metal-to-metal contact, overheating, and seizure on the more heavily loaded journal at the impeller end.

The following repair steps were accomplished by MTI to return circulator 1 to the design specifications:

1. lapped the spherical end of the pivots on the bearing pads to a true sphere;
2. removed the chrome-oxide coating from the damaged bearing pads, then recoated and reground the surfaces to the correct inside diameter;
3. relapped the undamaged pads to the correct inside diameter;
4. lightly lapped the rotor journals to remove any marks and to ensure their good condition; and
5. rebalanced the rotor to the drawing limits.

The reconditioned parts were returned to us and reassembled, and all circulators were operated satisfactorily during the week of December 7, 1981, but only after some vicissitudes.

On initial startup (after the bearing repair) with three circulators operating, an indication of journal bearing instability again occurred on circulator 1 around 14,000 rpm. Circulator 1 was now installed in pressure vessel 2. This seemed to rule out helium inlet conditions as a contributor to the apparent instability. Further, the journal bearing load pad clearance was exactly according to the design specification, and the rotor balance was nearly perfect.

Finally, two Wayne-Kerr signal conditioning boxes, supplied by the MTI representative, were connected directly to the two capacitance probes in the fan end journal bearing on circulator 1. Thus the very long instrument leads from the circulator to the CFTL signal conditioning equipment were bypassed. This change completely eliminated all electrical noise on the oscilloscope readout for this bearing, and there was no longer an indication of instability for this bearing at any speed up to and including the maximum of 23,500 rpm. However, random noise indications still occurred on the oscilloscope readouts for other journal bearings. Now that we were confident that the indicated bearing instability was not real, we were able to reach full speed with all three circulators operating.

Correcting the electrical noise on all 21 instrument leads from the circulator bearing probes to the signal conditioning equipment is planned as funding becomes available.

9.4.4 Circulator Power Supplies — E. M. Lees

After we improved the cooling air flow characteristics within the cabinets, each power supply was successfully operated for a minimum of 5 h while connected to a dummy load equal to the rated capacity of the power supply. During the early stages of circulator shakedown, two supplies had failures of solid state components, which were replaced from spare parts inventory. No further significant problems with the supplies occurred during the remainder of the circulator shakedown operations.

9.4.5 Instrumentation and Controls Shakedown — P. G. Herndon

Operational assistance provided during shakedown was focused mainly on achieving continuous simultaneous operation at full speed of all three of the CFTL helium circulators. Initial startup of the instrumentation and controls revealed no more than the usual number of process switches and control interlocks that required adjustment before normal operations could be sustained. However, the capabilities of the programmable controller, which was used to implement the protective interlock logic, permitted making convenient diagnosis of the problems and provided for making proper adjustments without needing to alter the circuits physically.

The most serious and aggravating problems were associated with the appearance on several sensor signals of the electromagnetic interference and radiofrequency interference noise generated by the operation of the silicon-controlled rectifiers in the variable-frequency power supplies for the helium circulators. Although some noise was expected, the strength of the actual noise was surprising and caused some degradation, particularly to the film thickness signals for the circulator bearings, which consist of 18 signals that are read out on three oscilloscopes. The effects were reduced sufficiently to permit the satisfactory shakedown of the circulators by the application of electronic signal filtering, by rerouting some signal cables, and by providing greater isolation of the signal cable shield and grounding system. A number of measurements of temperature and circulator speed and power were adversely affected, but these were also resolved sufficiently to achieve satisfactory operation of the loop. Additional planned revisions to the signal shielding and grounding system should eliminate most of the interference.

The as-built revisions to all the instrumentation and control drawings and revisions of other documentation were completed and approved.

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10. APPLICATION AND PROJECT ASSESSMENTS

P. R. Kasten

This chapter covers a variety of assessments and evaluations. The major effort was placed on High-Temperature Gas-Cooled Reactor (HTGR) application to fossil fuel conversion processes; other areas included evaluation and dynamic modeling of HTGR energy transport systems, review of program documents covering safety and lines-of-protection planning, and evaluation of core outlet temperatures for advanced HTGRs. Also, work was initiated on the evaluation of modular, small high-temperature reactors for process heat applications.

10.1 HTGR APPLICATION TO FOSSIL CONVERSION PROCESSES — P. R. Kasten and W. R. Gambill

Two papers were written and presented at technical meetings during the year. The first was presented at the Intersociety Energy Conversion Engineering Conference (IECEC) meeting in Atlanta in August 1981;¹ the abstract follows.

High-Temperature Gas-Cooled Reactors (HTGRs) permit nuclear energy to be applied to a number of fossil fuel conversion processes. In general, these processes require application of a steam-methane reformer; for such applications, an HTGR with an outlet coolant temperature ranging from 850 to 950°C is required. Such systems are applicable to chemical production, to a number of fossil conversion processes, and to chemical heat pipes. A relatively early application of reformer HTGRs might be the steam reforming of methane to produce synthesis gas for chemical production. At the same time, one of the coal conversion processes examined has energy requirements such that a steam-cycle HTGR having an outlet coolant temperature of 750°C will provide about 90% of the total primary energy requirements. In general, screening studies of coal and oil shale conversion processes indicate that coupling HTGR technology with fossil conversion technologies can lead to significantly reduced fossil fuel consumption and to reduced pollutant generation.

The second paper was given at the American Society of Mechanical Engineers meeting in Washington, D.C., in November 1981;² the abstract follows.

High-Temperature Gas-Cooled Reactors (HTGRs) permit nuclear energy to be applied to processes presently utilizing fossil fuels. Promising applications involve cogeneration, thermal energy transport using molten salt systems and (in the longer term) chemical heat pipes, steam reforming of methane for chemical production, coal and oil shale liquefaction or gasification, and eventually as the energy source for the production of thermochemical hydrogen from water. The potential market for process heat HTGRs by the year 2020 is estimated to be about 150 to 250 GW(t) for process heat-cogeneration application plus 150 to 300 GW(t) for application to fossil conversion processes. HTGR cogeneration plants appear attractive for new industrial plants that use large amounts of process heat. HTGR reformer systems will take longer to develop but are applicable to chemical production, to most fossil conversion processes, and to chemical heat pipes.

Estimates were made of the approximate capital costs of drilling for natural gas versus those for producing substitute natural gas (SNG) from coal via either all-coal or HTGR-coal hybrid processes. The conclusion (for the near term, at least) is that private U.S. energy companies are likely to risk relatively little of their own capital on coal-based SNG in comparison with expanded exploration-production efforts. The capital costs derived and their bases are summarized below.

10.1.1 Case 1 — Deep Wells

The average 1980 U.S. oil-gas well total drilling cost was approximately \$2460/m (\$750/ft). Escalated to fourth-quarter 1981 dollars at 16% per annum, the cost is \$2756/m (\$840/ft). We assumed that two high-flow-rate wells would produce the reference production of $7.2 \times 10^6 \text{ m}^3/\text{d}$ ($250 \times 10^6 \text{ scfd}$), that the success rate would be only 10%, and that, for 4572 m-deep (15,000-ft) wells, the unit drilling cost is double that given above [$\$5512/\text{m}$ ($\$1680/\text{ft}$)]. The resulting investment is $\$0.50 \times 10^9$, or \$75,480 per daily cubic meter (\$12,000 per daily barrel) oil equivalent of capacity.

10.1.2 Case 2 — Exxon Catalytic Coal Gasification Process (All Coal)

Adjustment of the September 1981 Bechtel estimate³ [which was based on the October 1979 Exxon projection,⁴ fourth-quarter 1981 dollars, and a $7.1 \times 10^6 \text{ m}^3/\text{d}$ ($250 \times 10^6 \text{ scfd}$) production rate] gives a capital investment of $\$3.08 \times 10^9$, corresponding to $\$466,466/\text{m}^3$ per day ($\$74,160/\text{bpd}$) of capacity.

10.1.3 Case 3 — Exxon Catalytic Coal Gasification Process (with HTGRs)

Bechtel's September 1981 design³ utilizes twin 1170-MW(t) 700°C steam cycle-cogeneration (SC-C) reactor units. The fourth-quarter 1981 capital cost is $\$4.68 \times 10^9$, $\$706,493/\text{m}^3$ per day ($\$112,320/\text{bpd}$) of capacity.

10.2 DIFFERENTIAL ECONOMIC ESTIMATES OF HTGR APPLICATION TO FOSSIL FUEL CONVERSION PROCESSES — W. R. Gambill

The economic attractiveness of HTGR applications to fossil fuel conversion processes was evaluated on the relative basis of the values of the decreased fossil fuel consumption when HTGRs were applied. A fixed production rate of 7949 m^3 per stream day (50,000 bbl) of fuel oil equivalent (FOE) was used. In this context, the most promising conversion processes are those with the largest values of avoided feedstock costs; these values would be available for construction and operation of the HTGR systems. The results should be considered as indicative only; Exxon (and others) have noted that there are approximately 90 economic-financial parameters and assumptions that must be established to define detailed product cost estimates fully.

The assumptions used were

- mine-mouth plants (negligible feedstock transportation costs),
- plant operating lifetime of 30 years,
- mined oil shale cost of $\$0.0132/\text{kg}$ [$\$12/\text{short ton (ST)}$],
- mined coal cost of $\$0.0330/\text{kg}$ ($\$30/\text{ST}$),
- natural gas price of $\$4.08/\text{GJ}$ ($\$4.30/10^6 \text{ Btu}$),
- cost in 1981 dollars,
- price inflation and scarcity escalation rates of zero each,
- an interest rate (real rate in view of the preceding item) of 4% per annum,

- plant capacity factor (CF) of 90%, and
- annual compounding.

The present value PV of a uniform series of amounts (in this case, of avoided costs) is calculable from

$$PV = R[(1 + i)^n - 1]/[i(1 + i)^n] ,$$

in which R is the dollar amount per year, i is the interest rate, and n is the subject number of years (30). As stated above, annual compounding was used.

To date, all studies have used only one or two HTGRs and an overall plant capacity factor of 90%. The same value is used here for consistency, but we believe it is unrealistically high. Assuming HTGR and conversion plant capacity factors of 70 and 90%, respectively, and the use of one, two, and three HTGRs would give overall plant capacity factors of 63, 82, and 88%, respectively. The economic impact of lowering the capacity factor would be to lower the amount of the present value available to finance the nuclear island.

The overall results are given in Table 10.1. Details of the cases are given below.

10.2.1 Case 1 — Exxon Catalytic Coal Gasification (ECCG) Process, Illinois No. 6 Coal Converted to SNG

Exxon's reference commercial plant design, scaled to 316,500 GJ/d (300×10^9 Btu/d) of SNG [7949 m³ per stream day (50,000 bbl)(FOE)], requires 19.1×10^6 kg per stream day (21,058 tons) of coal. For a 700°C SC-C, the net process energy substitution is $(0.86)(0.30)(100) = 25.8\%$; for a 950°C very high temperature reactor (VHTR), it is $(1.00)(0.30)(100) = 30.0\%$. From the preceding present value equation and with $i = 0.04$ and $n = 30$, the present value = 17.292 R . The sample format in Table 10.2 is representative of that used for each case.

10.2.2 Case 2 — Highly Modified H-Coal Process, Western Kentucky Bituminous Coal Converted to Light to Heavy Oils, High-Btu-Coker Gas, and Char

The highly modified H-Coal process utilized a 3000 MW(t) VHTR [910°C indirect cycle (IDC)]. United Engineers and Contractors' (UE&C's) all-coal

Table 10.1. Present value of HTGRs for fossil conversion processes [for production rate = 7949 m³ per stream day (50,000 bbl) of fuel oil equivalent (FOE)]^a

Case	Process ^b	Product	HTGR ^c	Based on evaluation by ^d	Approximate HTGR MW(t)	PV ^e (\$10 ⁹)	PV/unit power [\$10 ⁶ /MW(t)]
1	ECCG	Substitute natural gas	2 SC-Cs 2 VHTRs ^f	BGI BGI	1600 1850	0.926 1.076	0.58 0.58
2	H-coal (modified)	Semicrude mix	1 VHTR	UE&C SWEC	810	0.402	0.50
3	H-coal (standard)	Raw oils and substitute natural gas	1 SC-C	GA	580	0.767	1.32
4	SRC-II	Raw oils and substitute natural gas	1 SC-C 1 VHTR	GA GA	670 700	0.534 0.553	0.80 0.79
5	SRC-II	Finished transport fuels	2 VHTRs	GA	750	1.294	1.73
6	TOSCO-II	Pipelineable oil from oil shale ^g	1 VHTR	GE	910	1.117	1.23
7	Steam reforming of natural gas	Hydrogen Methanol	1 VHTR 1 VHTR	ORNL ORNL	2465 1239	4.94 2.48	2.00 2.00

^aReactor-process capacity factor, 90%.

^bECCG, Exxon Catalytic Coal Gasification; SRC, Solvent Refined Coal; TOSCO, The Oil Shale Co.

^cSC-C, steam cycle-cogeneration; VHTR, very high temperature reactor.

^dBGI, Bechtel Group, Inc.; UE&C, United Engineers and Contractors; SWEC, Stone & Webster Engineering Co.

^ePV, present value.

^fNo reformer needed.

^gPartially finished transport fuel.

Table 10.2 Representative sample format

	All coal	Steam cycle- cogeneration	Very high temperature reactor
Coal used, kg/year (tons/year)	6.27×10^9 (6.918×10^6)	4.66×10^9 (5.133×10^6)	4.39×10^9 (4.842×10^6)
Coal saved, kg/year (tons/year)		1.62×10^9 (1.785×10^6)	1.88×10^9 (2.075×10^6)
Coal saved (30 years), kg (ST)		4.86×10^{10} (53.54×10^6)	5.65×10^{10} (62.25×10^6)
Dollars saved per year		53.54×10^6	62.25×10^6
Present value, dollars		0.926×10^9	1.076×10^9
Approximate HTGR MW(t) [for two HTGRs (Bechtel Group, Inc., selection)]		1600	1850

design (summarized earlier),⁵ when scaled to 7949 m³ per stream day (50,000 bbl) (FOE), requires 14.66×10^6 kg per stream day (16,158 tons) of coal. The net process energy substitution (percent reduction in coal consumption) is only 14.6%. The present value, calculated in the same way as in case 1, is given in Table 10.1.

10.2.3 Case 3 — Conventional H-Coal Process, Coal Converted to a Raw-Oil Mixture and SNG

General Atomic Company's (GA's) HTGR-case design,⁶ scaled to 7949 m³ per stream day (50,000 bbl) (FOE), requires 13.43×10^6 kg per stream day (14,809 tons) of coal. The net reduction in coal consumption for an equal production rate is 23.3% for the HTGR case. The results are summarized in Table 10.1. This case is relatively much more promising than case 2.

10.2.4 Case 4 — Solvent Refined Coal-II Direct Liquefaction Process, Coal Converted to Heavy Fuel Oil (HFO), Light Distillate, Liquified Petroleum Gas (LPG), and SNG

For a 7949-m³ (50,000 bbl) (FOE) output per stream day, the coal feed rates for the process stream-cogeneration (PS-C) (base case), all-coal, and VHTR plants are, respectively, 13.88×10^6 , 16.72×10^6 , and 13.78×10^6 kg

per stream day (15,303, 18,437, and 15,192 tons). The corresponding PS-C and VHTR process-energy substitutions, compared with the all-coal case, are 17 and 17.6%, respectively. The resulting present values are given in Table 10.1.

10.2.5 Case 5 — Solvent Refined Coal-II Direct Liquefaction Process, Including Complete Upgrading of the Raw Oils to Finished Transport Fuels

Upgrading thermal efficiency and volumetric yield are assumed to be 86 and 96%, respectively. This case also assumes a totally integrated facility with a VHTR process energy substitution of 34% (compared with GA's estimate of 36% maximum). The pure fossil plant requires 20.26×10^6 kg (22,331 tons) of coal per stream day. The present value of $\$1.294 \times 10^9$ (Table 10.1) is the second largest of the seven cases examined, but the energy substitutability is also probably the most conjectural and may be too large.

10.2.6 Case 6 — The Oil Shale Corporation-II (TOSCO) Surface Retorting Process, Western Oil Shale Converted to Raw Shale Oil Hydrotreated to Pipelining Specifications

Based on General Electric Company (GE) estimates presented at the October 1981 Application Working Group Meeting at Bechtel, an 850°C very high temperature reactor (VHTR) would reduce oil shale consumption by about 21%, that is, from 63.69×10^6 kg per stream day (70,213 tons) for an all-shale plant to 50.31×10^6 kg per stream day (55,468 tons) for a VHTR plant. The present value (Table 10.1) ranks third, but would easily rank second if the shale consumption rate were lower for the VHTR case by 30 to 35% (rather than 21%), as in Davy-McKee's November 1981 report to GA concerning the application of an 850°C/IDC VHTR to Paraho-Indirect shale retorting,⁷ which included severe hydrotreatment to produce a high-quality syncrude. Even so, the capacity factor differences addressed at the outset must be resolved to reach clearcut conclusions.

10.2.7 Case 7 — Convective Reforming of Natural Gas to Synthesis Gas ($H_2 + CO$) Converted, for Example, to Hydrogen, Methanol, or Gasoline

The natural gas cost was taken to be $\$4.08/\text{GJ}$ ($\$4.30/10^6$ Btu). The reforming would be most effectively performed with a 950°C direct-cycle

(DC) or IDC HTGR reformer (HTGR-R) or with an 850°C DC reactor. The present value per unit of thermal power of \$2 million per MW(t) is the highest of any of the cases examined and is indicative of the economic potential of this processing route. The hydrogen production rate would be very large, $26.30 \times 10^6 \text{ m}^3$ per stream day ($926 \times 10^6 \text{ scf}$); for a more reasonable rate of $2.84 \times 10^6 \text{ m}^3$ per stream day ($100 \times 10^6 \text{ scf}$) of hydrogen, only 266 MW(t) would be needed, and a modular reactor would be appropriate. The HTGR process energy substitution was taken to be 46%, which is the percentage of natural gas used as fuel in a fired steam-methane reformer used to produce hydrogen. The energy substitution for the methanol product case (Lurgi low-pressure synthesis) was 24%, and the production rate was also quite high, $13.96 \times 10^6 \text{ kg}$ per stream day (15,393 tons). A large modern plant producing $2.27 \times 10^6 \text{ kg}$ (2500 tons) of methanol per stream day would require only 201 MW(t) (modular HTGR-R).

These comparative results generally coincide with the earlier consensus of the Application Working Group [members are from Bechtel Group, Inc., GA, Gas-Cooled Reactor Associates (GCRA), GE, and ORNL] in the sense that the most economically attractive HTGR applications are to shale oil production and upgrading, direct coal liquefaction and upgrading, and the ECCG process for SNG from coal. However, the results here are more definitive and include a preliminary evaluation of steam-natural gas reforming for chemical production. The latter process looks like the most promising for early VHTR application.

The above considers the benefits accruing from HTGR use. On the basis of the results obtained, the most promising VHTR applications are to steam reforming of natural gas for chemical production, followed by application to the SRC-II and TOSCO-II processes (with product upgrade). The most promising HTGR-SC-C applications are the H-Coal (standard) and SRC-II processes (no product upgrade).

In addition to potential benefits, costs of HTGR applications need to be determined. We recommend that emphasis be placed within the national HTGR program on obtaining (1) a detailed cost analysis of HTGR application to chemical production by steam reforming of natural gas and (2) a more detailed cost analysis of HTGR application to the H-Coal (standard) process, including product fuel upgrade.

10.3 HIGH-TEMPERATURE GAS-COOLED REACTOR REFORMER STUDIES AND PROCESS HEAT TRANSPORT MODELING STUDIES — S. J. Ball

10.3.1 Review of Thermochemical Pipeline System Costs — K. F. Wu

An ORNL report by Stovall et al.⁸ and a GE report by Vakil et al.⁹ were reviewed to evaluate the energy transmission costs of a thermochemical pipeline (TCP). The ORNL report considered the TCP system given by GE and used as base values the major equipment costs of GE (i.e., those costs for the reformer, methanator, and heat exchangers). However, there were significant differences in other economic factors; these differences specifically involved the (1) system reliability factor, (2) reformer and methanator capacities, (3) off-site pumping-station requirements, (4) pipeline layout, (5) allowable pipeline pressure drop, (6) process efficiency, (7) piping cost contingency factor, (8) pipeline right-of-way cost, (9) commercial availability of large-size pipes, and (10) compressor type and its cost. Of the above, the factors most responsible for the difference in TCP energy transmission costs as calculated by ORNL and GE were the piping and transportation systems, with ORNL estimates for the systems being three times higher than the GE estimates. Differences were specifically identified as follows:

1. A 30% contingency factor was added to the piping cost by ORNL, but none was added by GE.
2. A single right-of-way cost was used by GE for installation of multiple pipelines in a trench, whereas ORNL used a cost about 50% higher than that of GE.
3. An off-site pumping station was added by ORNL whenever the fluid pressure decreased by 1/10 of its starting pressure, but GE did not include this item.
4. A maximum pipe diameter of 1.22 m (48 in.) was considered by ORNL because of its commercial availability, but GE assumed that a 1.37-m-diam (54-in.) pipe would become available commercially.
5. ORNL used a pumping energy supplied through burning methane at 31% combustion efficiency and 85% compressor efficiency, but GE used electricity for compression.
6. The unit compressor cost estimated by GE was about \$100/kW, but ORNL's value was about 30% higher.

Of the above differences, items 1, 3, 4, and 6 were most significant. We concur with the estimates by Stovall et al. and conclude that the GE estimates are based on unrealistic assumptions. On the basis of the ORNL estimates,⁸ TCP energy transmission costs are too high to compete with alternative transmission systems for an estimated time of at least three decades.

10.3.2 Process Heat Reactor Dynamics Studies — S. J. Ball,
N. E. Clapp, Jr., J. C. Conklin, W. T. King, and
J. A. Mullens

The objective of the reactor dynamics work is to develop independent evaluations of dynamics, control, and safety problems of advanced-concept High-Temperature Reactor (HTR) designs, a continuation of a task that began in August 1980. Initial efforts were directed to development of HTR reformer (HTR-R) plant designs and their chemical heat pipe (CHP) systems. The task subsequently added consideration of the Sensible Energy Transport and Storage (SETS) plant, which was found to be economically attractive for certain process heat applications.

The program was reduced significantly in October 1981 because of budgetary limitations. Work on the HTR-R simulation had progressed to the point that operational simulations of most of the major components of the plant had been completed, including the reactor core, intermediate heat exchanger (IHX), once-through steam generator, steam reformer, turbine generator, and pipeline. The major portion of the plant not successfully modeled was the process gas heat exchanger train. The reference design for this section was modified during the year to incorporate packed-bed towers; however, we were unable to obtain sufficient design information for these special high-temperature, high-pressure towers to enable development of dynamic models.

Work on the dynamic simulation of the reference design SETS plant was completed, including development of load-following control schemes. This work has been reported.¹⁰

10.3.2.1 Reformer High-Temperature Reactor Simulation

The computer codes developed in the Nuclear Regulatory Commission (NRC)-sponsored HTGR Safety Program to simulate the Fort St. Vrain (FSV) reactor's core (CORTAP),¹¹ the once-through steam generator (BLAST),¹²

and the steam turbine plant (ORTURB)¹³ were adapted to simulate the dynamics of the corresponding HTR-R components. The core model is a simpler adaptation of CORTAP and includes two radial nodes for the fuel plus the moderator, with five axial sections overall for the fuel plus the reflector sections. As an option, either a six-delay group neutron kinetics model or power controlled by a process control system can be used. The parameters used for the core model were scaled up from the 842-MW(t) FSV reactor core. Calculations with the prescaled model had been shown to give excellent agreement with data from FSV reactivity ramp insertion tests.¹⁴ In the steam generator model, some of the design data, particularly tube bundle geometry, had to be modified to make the transition from FSV. Unlike the FSV, the HTR-R steam generator has no reheater section.

Additional development work was done on the steam methane reformer simulation, although a satisfactory final version has not yet been completed. The problem lies in the formulation of the reforming rate equation. The one used in the DSR-1 code¹⁵ (developed from integral experiments for steady-state plant conditions) is not appropriate for the localized dynamic calculations required in the simulator. An "improved" equation having the attractive feature of giving a zero rate when the methane is in chemical equilibrium was obtained from Kernforschungsanlage Jülich (KFA). Unfortunately, for typical operating conditions, this equation yielded rates that were considerably higher than those predicted by the DSR-1 code and were therefore suspect. We are currently corresponding with KFA concerning access to a later version of the equation developed from their EVA-ADAM experiments. A draft report has been prepared on the reformer simulation, and its publication is awaiting resolution of the rate equation question.

Work on the CHP pipeline dynamics code, an adaptation of the American Gas Association (AGA) code PIPETRAN-4, was completed.¹⁶

Development of a simulation of the methanator section of the CHP system was terminated.

10.3.2.2 Sensible Energy Transport and Storage High-Temperature Reactor

Dynamic models were developed for a GA reference design of an 1170-MW(t) HTGR-SETS plant to utilize an indirect cycle, with molten salt

(HITEC) as the heat transport and storage medium. A secondary helium loop provides isolation of the primary coolant from the salt, which is to be pumped offsite to a variety of storage and process heat facilities. The preliminary control system design used for the simulation accommodated both load flow and temperature changes. Additional considerations included minimizing the electrical power required for the two 30-MW helium circulators during reduced-load operation.

The molten salt transport and storage system model was based on a UE&C design¹⁷ and consisted of four sets of pump and storage units connected in series, with 11 km (7 miles) of piping between each station.

The core model used in the SETS simulator is essentially the same as that described previously for the HTR-R. The model for the motor-driven circulators was also derived from a previous FSV simulation,¹⁸ with the head-versus-flow curves scaled up to the larger SETS plant size. Because the FSV circulators are steam-turbine driven, conversion to SETS required only the removal of the turbine section.

Because design details of the IHX were not available when the model was being developed, the simulation was derived from the rated performance characteristics. A special computer program was written to take inputs describing the desired steady-state performance and from them to calculate design parameters required for the dynamic model. The resulting model used an eight-axial-section approximation, determined necessary to simulate the distributed-parameter characteristics of the IHX over its expected operating range.

As with the IHX, a design code was written to obtain parameters for the helium-salt heat exchanger, for which a 15-section model was required. For both heat exchangers, variable flows and temperatures are accommodated with heat transfer variations included as functions of flow. Approximate physical properties of HITEC molten salt were used.

Each of the four molten salt pumping stations was modeled by storage tank mass holdup and heat balance equations, pipeline hydraulics and holdup equations, and characteristic equations for the pumps. Control systems maintain constant salt levels in each of the storage tanks.

The salt-to-salt process heat exchanger is of an arbitrary design and was included only to simulate a variable process heat load. The reference model utilizes a four-section approximation.

A generalized model of a proportional-integral derivative (PID) controller with an operational feed-forward signal was used for each of the closed-loop control systems.

The report of the SETS plant simulation¹⁷ shows the reference plant to have excellent inherent and controlled dynamic characteristics for the primary, secondary, and salt loop systems. Further work on optimization and more sophisticated multiloop control schemes would be relatively straightforward, considering the inherent stability and simple control requirements of the plant.

10.4 OUTLET GAS TEMPERATURES FOR ADVANCED HTGRs — P. R. Kasten, P. L. Rittenhouse, C. J. Long, and W. R. Gambill

A summary evaluation of the appropriate outlet gas temperature for advanced HTGR applications was performed. For process applications, the outlet gas temperature required of advanced HTGRs is in the 850 to 950°C range. One of the primary applications of advanced HTGRs would be as an energy source for reformers; however, the outlet gas temperature is influenced by whether it is a DC or an IDC system (with an intermediate heat exchanger). Current industrial reformer practice utilizes temperatures from 760 to 870°C. At the upper end, an approximate 920°C outlet gas temperature would be required of HTGRs if a DC system were employed. If an IDC were used, a reactor outlet temperature of 950°C would result in reformer temperatures of about 850°C. If the outlet gas temperature were 900°C, the DC system would have reformer temperatures of about 835°C, and the IDC, about 775°C. It is not yet clear if a DC system is practical for licensing. A prudent approach would be the use of an IDC (or a modified IDC utilizing a duplex tube reformer), which implies a need of a reactor core outlet helium temperature of 900 to 950°C.

If the HTGR were employed in direct steam gasification processes, such as the Winkler process (noncatalytic), outlet reactor temperatures of at least 950°C would be required.

If the HTGR were applied to gas turbine systems, the following applies. Relatively extensive studies were made for an outlet gas temperature of 815°C;¹⁹ a thermal efficiency of about 36% was estimated. In a comparably detailed analysis of a gas-turbine (GT)-HTGR but with an

outlet gas temperature of 850°C,²⁰ the thermal efficiency was estimated to be 40%. On the basis of cursory estimates, GA figures the efficiency of a 950°C GT-HTGR to be about 43 to 44% relative to 40% for an 850°C outlet temperature. All the above thermal efficiencies are based on dry cooling. Use of the Carnot equation for thermal efficiency, adjustment of the heat rejection temperature so that an outlet temperature of 850°C gives 40% thermal efficiency, and use of that adjusted rejection temperature for the 950°C outlet temperature give a thermal efficiency of about 45%. Thus, a significant (10%) improvement in thermal efficiency appears to be possible by going to a 950°C outlet helium temperature in a GT-HTGR.

The above does not consider materials problems. The strength of current materials at both 900 and 950°C is low, although it is, of course, lower at 950°C. The carburization problem, however, is probably no more difficult at 950°C than at 900°C if water is used to control carburization.

Overall, it would undoubtedly be easier to develop a material for 900°C application than for 950°C application. However, current information is insufficient to conclude that the process applications at a 900°C outlet temperature should be the objective of the VHTR program. It would be prudent to maintain the objective of a 950°C outlet gas temperature.

10.5 REVIEW OF HTGR SAFETY STUDIES BY GENERAL ATOMIC — D. L. Moses

To understand the bases on which a safety program should be developed and to comment knowledgeably on proposed "lines of protection" (LOP) approaches, a review was made of previous safety studies performed by GA in their Accident Initiation and Progression Analysis (AIPA) studies. This report reviews the current status and direction of the DOE-sponsored HTGR Safety Program and recommends a change in emphasis to the proposed program plans, which are currently stressing the use of probabilistic risk assessments (PRA). The work effort was directed to achieving a comprehensive overview of key issues without digging too deeply into details of the experimental and analytical methods and data that have been developed from years of HTGR safety research. In the following sections, specific problems with the use of probabilistic risk assessments in both the current and future HTGR safety research efforts are first discussed,

followed by a brief delineation of those areas that require continued or renewed attention in order to develop a safe, reliable, and licensable HTGR plant.

10.5.1 Problems in the Current Research Objectives

As proposed by both GCRA²¹ and GA^{22,23} future HTGR safety research is to be based on setting quantitative safety-reliability risk goals and demonstrating the achievement of those goals by probabilistic risk assessment. We anticipate that this approach will be used both in addressing licensing issues and in selecting priorities for emphasis on competing needs in safety design and research. The objective is to emphasize safety with economy and to avoid unnecessary expensive research and development in bringing the HTGR into the commercial market.

Although there are many promised advantages for using risk goals for assessing the value impact of research and development decisions, there are at least three reasons for being very cautious in adopting this approach for the HTGR: (1) the limitations to (if not a total lack of) the data base for component failure and external event probabilities, (2) the implications of societal and regulatory perspectives on severe accident risk, and (3) the possible industry-engineering management misconception of probabilistic methods as a substitute for more traditional quality control procedures. These specific issues are addressed in the following discussion. Similar and other generic problems with the use of quantitative risk goals have been addressed previously in both DOE-sponsored and NRC-sponsored studies.²⁴⁻²⁶

10.5.1.1 Component Failure and Event Probabilities

The probabilistic risk assessment methodology is composed of several parts. Accident delineation (or accident event sequence identification) and consequence assessment (two parts of safety analysis) are performed independent of the knowledge of either component failure rates or external event frequencies (e.g., earthquakes). Both of these parts are essential to safety analysis, whether deterministic or probabilistic. The use of safety analysis accounting for component or system failure rates and event probabilities has been a major issue in light-water reactor safety and has also been a major research endeavor for the HTGR.

Light-water reactor experience

The *Reactor Safety Study*²⁷ represents perhaps the first comprehensive attempt to combine accident delineation and consequence assessment with the estimated frequency of sequential failures leading to core melt. The purpose of the *Reactor Safety Study* was to obtain an estimate of the relative risks of operating light-water reactors in the United States. Substantial technical reviews^{28,29} of the *Reactor Safety Study* have been critical of the use of certain failure probabilities such as the use of the same failure rate for a component with different failure modes under different conditions and also the inadequate treatment of partial failures. The same reviews have recognized that failure probabilities are often based on engineering judgment when statistically satisfactory operational data are lacking, which is typical of very low probability events.

As to the overall adequacy of the failure probabilities used in the *Reactor Safety Study*, the NRC-appointed Lewis Committee found that, "we are unable to determine whether the absolute probabilities of accident sequences in WASH-1400 [ref. 27] are high or low, but we believe that the error bounds on these estimates are, in general, greatly understated." The Lewis Committee was also critical of the lack of an adequate peer review process. On the other hand, the review sponsored by the Electric Power Research Institute (EPRI), representing the interest of the utilities, found the *Reactor Safety Study* to be too pessimistic (i.e., conservative) for mean values for sequential failure probabilities and argued that proper peer review of engineering judgment can lead to an actual reduction in the uncertainty bounds.³⁰

More recently, however, the *German Risk Study*,³¹ which follows the methodology of the *Reactor Safety Study* but draws on German operational experience, has indicated that, "In general, greater expected values (on medians) and greater uncertainty factors of failure rates or probabilities were found than in WASH-1400 [ref. 27]." As expressed in the foreword to the English translation of the *German Risk Study*,³¹ the position of the Electric Power Research Institute is that, "the German team . . . has probably performed an in-depth peer review of the *Reactor Safety Study*"

and that the comparability of conclusions "does not guarantee validity" but "the similarity does lend confidence that the *Reactor Safety Study* contains no major errors." In the same foreword, the following statement also appears:

The benefit of a probabilistic risk assessment stems from identifying the accident sequences that dominate public risk rather than from stating the "bottom line" risk.

In other words, the unacknowledged technological softness and the apparent lack of consensus with regard to the failure and event probabilities make it extremely difficult to set goals (i.e., "bottom line" risks) for light water reactors but do not preclude the use of this semiquantitative tool in augmenting engineering judgment in the selection of the more important accident sequences for safety research.

Most recently, the NRC-sponsored studies^{25,26} of the application of quantitative risk criteria to licensing have identified a number of problems to be resolved for using data on component and system failure rates, which some NRC people would rename "component or system unavailability." A key area of concern is that "overall quantitative goals may introduce severe verifiability goals with respect to low probability-high consequence events."²⁶ Other areas of concern are the lack of consensus among the experts on appropriate data bases and the current lack of an adequate tracking system for component field experience.²⁵ The studies also recognize that the frequency of component failure may be time dependent so that a single number may not reflect the effects of age or periodic maintenance. So far, the NRC work has been limited to identifying problem areas and alternative strategies for implementing various types of risk goals. No firm commitment has been made to implementing specific quantitative risk goals, nor has any reasonable estimate been made for the cost of obtaining the necessary data bases to support such an effort.

HTGR applications

Following the same general approach used in the *Reactor Safety Study*, GA under U.S. Department of Energy (DOE) sponsorship has instituted an

ongoing probabilistic risk assessment program for its proposed HTGR steam cycle power plant. The results of this program have been published in a series of status reports entitled *HTGR Accident Initiation and Progression Analysis*, AIPA.^{32,33} As indicated above, the methodology used in the AIPA is generally the same as that used in the *Reactor Safety Study*. However, the estimates for failure and event probabilities and uncertainties in the *Reactor Safety Study* were based on light-water reactor operational experience as much as possible, whereas the estimates used in the AIPA are termed "prospective" and rely more on engineering judgment for many as yet unbuilt and untested components. General Atomic characterizes these estimates as "realistic" and argues that attention has been given to European gas-cooled reactor experience and fossil-fired plant component data in making these estimates.

Aside from the numerous comments about earthquake frequencies, the AIPA peer reviews have by our count elicited only nine comments on component failure probabilities, and four of these concern reheater or steam generator tube failure rates. The low response may be credited to the lack of detail in most cases as to the origin of the quoted failure probabilities coupled with the lack of experience among most reviewers in making such estimates. Without the requisite detail to engender questions, the reviewers as reflected by the comments tended at most only to compare an AIPA number to one used in the *Reactor Safety Study* rather than to question the rationale for using a given estimate. In Sect. 2.3 of vol. VIII to the AIPA status report,³² GA expressed the opinion that the AIPA component failure rate data were not as important as in traditional reliability analyses because risk assessments tended to deal with higher levels of assembly (i.e., systems) that would be unaffected by changing data in the lower levels of assembly (i.e., components). The additional argument was made that the methodology being used in the AIPA would tend to pinpoint data deficiencies. The first point is true if the change in the median value of the component failure rate is small compared to uncertainties and if care is taken not to quote median values as best estimates without qualifying them with uncertainties. The second point

is misleading because engineering judgment is being used in many cases to estimate both median values and uncertainties, and engineering judgment can only be questioned by an adequate peer review when the bases for such judgment are adequately documented. Three examples are explored.

Steam generator and reheater tube failure rates. As stated previously, four review questions were raised on steam generator or reheater tube failure rates. The steam generator tube failure event results in water ingress to the primary system, which may result in graphite and fuel particle damage by hydrolysis, thereby releasing fission products. Water ingress was estimated to be at least an order of magnitude less limiting in risk than the release of primary coolant contamination directly to secondary plant water by way of a reheater tube leak failure. The reheater tube leak constituted the limiting low consequence-high probability accident identified in the AIPA, and developing an understanding of reheater tube failure mechanisms and probabilities constituted two of the nine research and development recommendations originating in Phase I of the AIPA. The subsequent removal of the reheater from the conceptual plant design in 1979 eliminated concern over reheater tube failure.²² The 1979 redesign replaced steam-driven circulators with electrically driven circulators to reduce water requirements for circulator bearings, thereby mitigating a water ingress mechanism that has plagued the Fort St. Vrain plant.^{34,35} However, the redesign does not appear to be in response to risk levels identified in the AIPA.

In the preliminary analysis (vol. III of the AIPA status report)³² the impact of steam generator tube rupture was minimized because a failure rate of only 3×10^{-3} per reactor-year was estimated. In response to the peer review comments, the failure rate in Phase II was raised to 0.1 leaks per reactor-year, thereby increasing the risk of the limiting accident sequence to within about a factor of 10 of that for the reheater tube failure. The second estimate for failure rate was taken from essentially the same data base (a different undocumented

reference) as the first, and both utilized Magnox gas-cooled reactor operational data corrected for the large number of weld defects attributed to field construction.*

However, Table A1-2 in vol. IV of the AIPA status report indicates that carbon monoxide corrosion is the second major mechanism for Magnox steam generator tube failures, and this aspect is not a problem for HTGRs. [The carburization-oxidation mechanism in mild steels used in both Magnox and Advanced Gas-Cooled Reactor (AGR) steam generators has been described in two recent articles.]^{36,37} In addition, the AIPA status report fails to mention that Magnox boilers use finned tubes with thicker walls and operate at much lower steam temperatures and pressures than do HTGRs.³⁸ Therefore, the data base used by GA does not appear to be applicable to HTGR steam generators.

A more recent data base is available in the operating experience of Babcock & Wilcox (B&W) once-through steam generators.^{39,40} These steam generators also operate at lower steam temperatures and pressures with shell-side rather than tube-side boiling; however, these steam generators are similar in producing superheated steam and in requiring 100% flow of feedwater through demineralizers before entering the steam generator in order to keep impurities at the parts-per-billion level. For HTGRs, the chemistry tolerance is even more important because tube-side boiling deposits sludge and scale inside the tube, where it cannot be knocked off by turbulence or frothing and allowed to settle elsewhere (as is possible in the B&W design). These deposits can reduce heat transfer, block flow leading to tube burnout, and increase wall corrosion. Experience with the steam generators at the three Oconee units and at Arkansas Unit 1 indicates that the tube failure rate averages about 0.8 leaks per reactor-year and tends to increase with time following an incubation period of a few years. The impact of periodic steam generator cleaning at these plants

*The uncorrected failure rate is 1.02 leaks per reactor-year. General Atomic also uses the same undocumented source for data from an unidentified Magnox plant to estimate the leak size probability distribution, that is, 0.9 per leak for flow rates between 0.000454 and 0.0454 kg/s (10^{-3} and 10^{-1} lb/s) and 0.1 per leak for flow rates between 0.0454 and 10.9 kg/s (10^{-1} and 24 lb/s).

has yet to be determined. In addition, in-service inspections at the oldest plant, Oconee Unit 1, have resulted in removing 148 tubes from service before failure because of tube wall thickness erosion. The higher steam temperatures and pressures in HTGR steam generators, coupled with a concern about erosion due to steam impingement inside helical tubes that may be partially blocked by scale or sludge, lead to an estimated tube failure rate of between 1.0 and 10.0 leaks per reactor-year, depending on GA's final recommendations for cleaning and in-service inspections. [A large test program has been reported in the United Kingdom to study erosion-corrosion in replicated geometries (e.g., return bends) in AGR steam generators.]³⁷

Assuming the same leak size distribution as used in the AIPA, this estimate makes the apparent risk of a steam generator tube failure equal to or greater than the original estimate for reheater tube leaks, which also shared questionable failure probabilities on the basis of the same Magnox data base. The steam generator materials analysts at GA have been aware of possible failure mechanisms for many years,⁴¹ but there appears to be little feedback into the risk analysis in spite of the initial concern over reheater tube failure rates. The next few years of promised high-power operation at Fort St. Vrain may aid in resolving some of the questions about leak rates in the steam generator, but the possibility of a longer incubation period for the failure mechanism provides little assurance at this time of estimating the upper limit risk over 40 years of plant life.

Turbine trip with loss of offsite power. Another example from the AIPA status report is the turbine trip rate used for the assessment of the loss of offsite power leading to a potential core heatup. In the response to comment A-5, Sect. 3.5 of vol. VIII to the AIPA status report³² (one of the nine comments referred to above), GA noted that foreign gas-cooled reactor experience was used to estimate main turbine-generator trip rates for load rejection during the loss of an offsite power event.* In

*General Atomic uses the terms "loss of offsite power" and "load rejection" in the same context to refer to loss of grid power to the plant accompanied by the loss of grid load (i.e., station blackout).

response to the same comment, GA cited in particular the loss of offsite power accident sequence, which was reduced from 1×10^{-6} to 3×10^{-7} in estimated annual occurrence rate by updating the data base from the Phase I to the Phase II analysis.

In the Phase II status report, GA described its data base for a turbine trip during load rejection as using the British gas-reactor experience of eight turbine trips out of 95 loss of offsite power events leading to an estimated rate of 0.1 trips per demand. General Atomic fails to acknowledge one key point, however, in that their design is based on using one main steam turbine generator to produce either 1160 MW(e) or 900 MW(e), depending on design. On the other hand, a typical British dual reactor Magnox station³⁸ uses six independent, much lower rated turboalternators (~94 MW each) plus three auxiliary, variable-speed turboalternators (~33 MW each), which are not connected to the grid and which provide the essential house load power to the gas blowers. (The above system description is that of the Hinkley Point A Station, which is apparently typical for the six stations achieving commercial operation between 1964 and 1968, except that the Sizewell and Oldbury stations⁴² use steam-driven rather than electrically driven gas blowers. Earlier Magnox stations were rated at a factor of 2 to 5 less in power levels, but this author did examine the turbine-generator arrangement for the older stations.) Because of the time frame in which the AIPA estimates were made, the data must be based on Magnox experience rather than on AGRs that use a single turboalternator [~625 MW(e)] associated with each of two reactors at a given site. The lower rating of each Magnox turbine greatly enhances its survivability during load rejection, and the multiplicity of main turbines plus independent auxiliaries practically guarantees survivability for the Magnox plant during a loss of grid power. It is unclear whether the eight trips in the Magnox data base refer to individual primary turbine trips or to the less likely total plant trip of all primary and auxiliary turbines. At any rate, the data base appears to be suspect for any practical application to GA HTGR design.

The Fort St. Vrain load rejection experience is based on a system using a reheater to reheat steam from circulator turbine drives. As a

result of an arbitrarily high runback rate of feedwater flow after initiating a ramp load rejection, high reheat steam temperatures have produced the plant trips experienced during 2-min power rollbacks from 70 to 25% power.⁴³ Although this problem is apparently solved for an orderly rollback, we were unable to determine if there is experience with step load rejections such as those that could be encountered in the event of a loss of the electrical grid system. Presumably the inability to accommodate large ramp load changes in the past has precluded tests for the response to step load changes.

Most light-water reactors, including those of a size comparable to the proposed HTGR plant, are designed to trip the turbine during load rejection to prevent turbine damage. There are some plants [such as H. B. Robinson Unit 2 (Westinghouse) and Crystal River Unit 3 (B&W)] that have been designed with a steam dump capacity to ride out a 100% to low-power step load rejection, assuming no loss of offsite power. However, Robinson has not been tested,⁴⁴ and Crystal River, which has been tested, has not been very successful in riding out the transient.⁴⁵ Regulatory Guide 1.68, Rev. 2, requires that light-water reactors demonstrate dynamic response to total station blackout (i.e., loss of offsite power) only in the 10 to 20% power range. The Westinghouse-designed Mihama-I Plant in Japan attempted a total station blackout test with a step load rejection from 75% power but tripped at 8 s into the transient on steam generator water level.⁴⁶

The use of a turbine trip rate of 0.1 trip per demand during load rejection accompanying total station blackout therefore appears to be unrealistic for an untested design. (A similar conclusion was reached by Washburn⁴⁷ in the informal NRC-sponsored review of the AIPA performed at the Los Alamos National Laboratory. Her conclusion was based on light-water reactor performance by use of the data base from the *Reactor Safety Study*. Apparently Washburn did not attempt to verify the British experience.) A value of 1.0 trip per demand is more suitable and results in increasing the Phase II annual rate of occurrence of the loss of off-site power accident sequence from 3×10^{-7} to 3×10^{-6} per reactor-year, which is 3 times greater than the value in Phase I, rather than less.

Failure rate data normalization. In Appendix III of the *Reactor Safety Study*,²⁷ care was taken in deriving system and component failure rates to account for the proper amount of time over which a component or system was challenged in either an active or passive mode of operation. Therefore, in the *Reactor Safety Study*, the failure rate data is reduced in almost all cases to an in-service-hour basis before scaling to a per-reactor-year basis. However, in cases in the AIPA wherein the actual data used in the failure rate assessment have been given, it appears that similar care has not been exercised in reducing the AIPA data.

For example, in the Phase II analysis of a possible core heatup resulting from a main loop cooling system fault, the failure rates used for the main condenser (Tables 4-7 and 4-8, ref. 33) and turbine trip (Table A2-1, vol. IV, ref. 31) are both normalized to the calendar year rather than the component in-service year. Proper normalization would increase the estimated failure rates for these systems between 10 and 20% without justifying a concurrent reduction in the associated uncertainty. In the same accident sequence, the more important failure rates estimated for the initiating events are also given in terms of either per-reactor-year or per-plant-year, but there is insufficient information to assess the procedure actually followed in normalizing these data. In this case, researching the references does not prove fruitful either. For example, the referenced report⁴⁸ for data on ruptures of feedwater pipes and main steam headers presented the equipment operating data in many different formats (such as hours, end dates, and year periods) and without an indication of the fractions of time in service. It was therefore impossible to generate a meaningful independent check without knowing precisely which information was used by GA.

Because failure rate data are frequently presented throughout the AIPA in per-reactor-year units, there is a concern that the precedent for not using accepted methods in normalizing the failure rates for sequential events has possibly biased the mean results in an optimistic, nonconservative direction. The mean values should reflect the proper mathematical processing of the best available data. It is improper to require the

estimated uncertainties to account for any lack of precision in evaluating mean values, particularly because the mean value is most often quoted by GA as the realistic best estimate for purposes of discussion, as in the loss of offsite power accident sequence discussed above. Although the effect of this problem is difficult to quantify without an in-depth audit, it is fair to say that there appears to exist a subtle discrepancy in the manner in which data were manipulated in the *Reactor Safety Study* and the AIPA. Part of this problem may be alleviated simply by improving documentation of the AIPA data base, but certainly more care is required in any future use of component operating data.

Conclusions

The preceding discussion represents a relatively superficial attempt to evaluate the HTGR data base for failure probabilities. This discussion indicates an inherent problem in the liberal use of so-called data bases that go undocumented and therefore unreviewed. Aside from the AIPA status reports, the documentation to date on GA data bases has constituted little more than a single bibliography without a sufficient assessment of the sources referenced.⁴⁹ This specific problem of data manipulation has been identified by other studies on the use of risk goals.²⁴ It is judged to be likely that, if all the estimates used in the AIPA status report were scrutinized, both the median values and the estimated uncertainties would increase, thereby increasing the estimated risk. General Atomic describes the AIPA as "realistic" and "prospective," but a better characterization is perhaps optimistic and in many cases unsubstantiated. The use of risk goals is questionable when initial estimates for selecting priorities can increase by several orders of magnitude, as has been demonstrated in the preceding discussion.

For the purpose of licensing, the data base issue is most critical because the probabilistic risk assessment will be opened to public scrutiny, including that of intervenors and nuclear power critics who may not be very forgiving in their review. One suspects that in such a case the positive efforts in the area of accident delineation will not receive the same attention as the failure rate data base with its uncertainty

estimates, which will be characterized as an attempt to quantify the lack of knowledge about system performance or to base safety on guesswork. A more positive approach is desirable, but it is not clear that simply changing the nomenclature for failure rate to that of unavailability achieves this end.

10.5.1.2 Risk and Societal-Regulatory Perspectives

Another potential problem with establishing probabilistic risk goals is that the assumed advantages of such goals may be eroded by societal and regulatory concerns. The utilities and the suppliers of reactor plants and components regard risk goals as advantageous in that there appears to be more flexibility in designing and building reactor plants to meet overall risk criteria rather than the traditional defense-in-depth philosophy. These parties hope to use the design conservatisms inherent in some component to avoid investing in expensive research and design efforts to upgrade other components and systems that are a part of the same postulated set of sequential failures leading to a severe accident scenario. Whether or not such advantages are realized by the nuclear industry depends on the action of the NRC, acting in the public interest. Recent publications by the GCRA such as a survey of numerical safety targets⁵⁰ appear to encourage the industry's efforts to pursue such goals in the light of NRC sponsorship of a plan to study the development of safety goals. However, in the aftermath of the accident at Three Mile Island Unit 2 (TMI-2), the NRC has been sensitized to public concern and has taken many actions in an attempt to enhance reactor safety. The NRC sponsorship of risk assessment studies and risk criteria development can be attributed in part to a desire to fill in the gaps in traditional safety analysis by augmenting it with diverse methodologies.⁵¹ As noted in a September 1981 article in *Nuclear News*,⁵² NRC has made no commitment to adopt probabilistic risk assessment in spite of the obvious interest that has been documented. At this time it is unclear whether industry will benefit from such studies, particularly since the NRC has not defined requirements for evaluating safety in degraded core conditions. In principle, arguments can be presented that the low radiation release from

TMI-2 and the low probability estimates for the occurrence of a degraded core condition are sufficient to preclude the need for upgrading plant protection and safety systems. However, safety-related plant modifications have been required as a result of TMI-2, and more expensive modifications may be imposed in the future in spite of the low probabilities involved.

The survey report on numerical safety targets⁵⁰ indicated that some apparent consensus is developing between industry and the NRC on the risk targets for preventing core meltdown ($<10^{-4}$ per year), for preventing significant harm to any individual member of the public ($<10^{-6}$ per reactor-year), and for implementing cost-effectiveness criteria (somewhere between \$1 million and \$10 million invested per death averted). However, there are two significant areas of disagreement between the industry and the regulatory agencies.

The first area of disagreement centers on the use of realistic-versus-conservative analysis methods. As interpreted in the GCRA-sponsored report,⁵⁰ the industry position is that the NRC should either authorize the use of realistic analysis methods or provide for easing the risk targets in the light of using acknowledged conservatisms in the analyses. On the basis of the information discussed in the previous section, the NRC may have good reason to question whether the industry tends to be too optimistic in so-called realistic analyses. The NRC is also aware that no standards or consensus exists for a data base for component and system failure probabilities.^{25,26}

The second area of disagreement concerns the target for societal health hazards; that is, what consequence limit constitutes significant public harm. The NRC societal risk proposal is that from all accidents there should be less than 0.4 early deaths per year and less than 2.0 delayed cancer deaths per year. Notably, the interim industry proposal as described in ref. 50 is a factor of 4 more conservative than that of the NRC. Both proposals recommend a formulation in which societal risk from a nuclear plant is defined as being equal to the summation over all accidents of the product of the estimated accident frequency times the actual number of deaths expected per accident, with the value for

expected deaths raised to a suitable exponential power to account for the perceived social impact of (or public aversion to) such deaths; that is,

$$\text{Societal risk} = \sum_{i=1}^I (\text{accident frequency})_i \times (\text{expected early deaths})_i^{\alpha},$$

where

α = risk aversion exponent estimated at either 1.2 (NRC proposal) or 1.5 (GA),

i = 1, I accident sequences required to be considered,

$(\text{accident frequency})_i$ = the mean frequency of sequence i over the time interval required to produce 10^{10} kWh of electrical energy.

This power law treatment of risk aversion is apparently based on the DOE-sponsored studies at the University of California at Los Angeles.^{53,54} These studies indicated problems in fitting limited data bases to a power law formulation as well as a large degree of variation in the exponent α , depending on the population and hazard state evaluated. An alternative treatment of risk aversion is suggested by J. A. Dukes of the British Institute of Nuclear Engineering.⁵⁵ His research in the United Kingdom indicates that the risks should be multiplied by an appropriate set of weighting factors (i.e., "evident emotive factors"), which account for the public response to and comprehension of certain life-threatening events. Dukes suggests that a multiplier of about 70 may be the appropriate risk avoidance factor to apply to the risk for accidents that might result in a core meltdown such as the loss-of-coolant accident in light-water reactors. However, the data bases for the quoted emotive factors are not documented.

Comparison of the power law proposal to that of the emotive weighting factor indicates that there is numerically a significant difference in alternative methods for assessing the risk aversion of the public and that neither method is well supported by data. Any attempt to introduce an

accounting of public risk aversion into the licensing process may well open a Pandora's box of disadvantages to reactor plant vendors and operators, a possibility recognized by the Atomic Industrial Forum.⁵⁶ The worst-case scenario is that social activities could pressure Congress or the NRC into requiring a quantification of the risk averseness of the population neighboring any operating or proposed nuclear facility. The diversity of possible results from such a procedure could counter the economic and safety advantages of standardized plant designs and force expensive upgrading on safely operating plants for nontechnical reasons. This dire prediction may be unlikely in any event but will be more unlikely if probabilistic risk assessment is given either a complementary or ancillary role in evaluating those areas of nuclear safety that require analysis to supplement the conservative defense-in-depth approach. The latter option is, of course, that being considered by the NRC,²⁵ that is, some mixture of both quantitative and qualitative risk assessment.

In conclusion, the use of probabilistic risk assessments may well become one of several required methods for satisfying the NRC licensing review process; however, the NRC interpretation with regard to meeting quantitative risk goals may involve work in addition to traditional analyses requiring a conservative defense-in-depth demonstration of plant safety. The status of risk goals is too premature for industry to count on deriving relief from design conservatism.

10.5.1.3 Miscellaneous Comments

A problem with any analytical tool is often the failure to communicate properly both its potential and its limitations to program managers, budget directors, and other responsible individuals charged with directing analytical work effort but not actually involved in the technical details of such work. The prospect exists that emphasis might be placed on an approach that is improperly credited with results that could have been achieved more economically otherwise or that obscure areas of true need. This situation appears to apply to some extent to the use of probabilistic risk assessment in the HTGR Safety Program.

For example, the GA status report *HTGR Safety Research Program*²³ implies that the AIPA is responsible for identifying the dominant HTGR

severe accident as being the unmitigated core heatup event. For this reason, GA is pursuing a set of ongoing deterministic studies on fission product release and plateout processes, failure modes of the primary coolant boundary, containment atmosphere response in mitigating fission product releases and withstanding pressure transients, and possibilities for recriticality due to high-temperature-induced damage to the shutdown mechanism. In fact, however, the potential severity of the HTGR core heatup event was recognized by the Atomic Energy Commission (AEC) several years before the AIPA was initiated,^{57,58} and the AEC required an evaluation of loss-of-cooling capability in the safety analyses for both the Peach Bottom and Fort St. Vrain HTGR plants. The deterministic studies currently being performed by GA on the system response to a core heatup can as readily be attributed to the logical consequences of applying a defense-in-depth philosophy to a possible catastrophic event. The definition of postulated event sequences and the use of statistical evaluations of data in kinetic processes (e.g., fission product release or plateout rates) are necessary to develop the mechanistic models of the accident, but engineering judgment is the key ingredient in selecting priorities for accident analysis. It was engineering judgment based on evaluating deterministic analyses submitted by GA that led to AEC recognition that the design basis depressurization accident (which releases normal operating contamination to containment but does not preclude core cooling at atmospheric pressure) is less serious than a long-term loss of all the redundant systems designed to maintain cooling capability. (General Atomic had initially postulated the design basis depressurization accident as the limiting event on the earlier plants, but it must be recalled that Fort St. Vrain does not have containment. Washburn contends that this accident has also been underrated in the AIPA.)⁴⁷ The results of the AIPA have confirmed the AEC judgment but have not supplanted it.

Another example can be found in the GA report *The HTGR Safety Program*.²² In this report, probabilistic risk assessment receives the major attention, with one item, code development, relegated to a single sentence on documentation and validation of safety codes. Similarly, the semiannual reports on the HTGR Plant (formerly Generic) Technology Program⁵⁹ provide brief descriptions of computer code development or

updates occurring during the respective reporting periods. In the past few years, the emphasis in these reports has been on reliability models. However, a review of the computer programs utilized in safety analyses indicates that a number of the deterministic codes that are used either directly in the safety analysis or in the system design effort that supports the safety analysis are as yet unverified and in many cases undocumented. Again, because of the emphasis on probabilistic risk assessment, most of the reliability and probabilistic codes have been so verified and documented; however, these latter codes are primarily computer programs for statistical manipulations of data rather than for the actual analysis of plant performance or accident phenomena. The actual numbers obtained from our review are tabulated in Table 10.3.

The seriousness of this situation is compounded by the fact that a recently published NRC-sponsored report⁶⁰ is critical of both GA and the Public Service Company of Colorado for failing to pursue "a sufficiently aggressive experimental verification program" on the computer codes currently used in the safety analysis for the Fort St. Vrain HTGR.

The emphasis on probabilistic methodologies as a possible money-saving device in safety and reliability analysis is apparently diverting the attention of GA's analytical effort away from the phenomenology of postulated accident sequences with the exception of the worst-case core heatup scenario. True safety is achieved by understanding event mechanisms and designing systems in order to prevent or to mitigate the consequences. Risk prioritization by probabilistic methods is too

Table 10.3 Reliability and probabilistic codes

Type	Code development status				
	Being used or developed Number	Documented		Verified	
		Number	(%)	Number	(%)
System design and analysis	137	29	23	11	9
Safety analysis	32	14	44	9	28
Reliability analysis	11	11	100	10	91

premature to justify the attention given it over that for the verification of the deterministic simulations currently required to assess plant safety.

Finally, the AIPA status report (vol. 1) acknowledges that probabilistic risk assessment is an attempt to satisfy the need for systems and safety analysis identified in the 1974 *Planning Guide for HTGR Safety*,⁶¹ which was produced in a joint effort between GA and ORNL. However, in the response to comment A-3 in Sect. 3.1 of vol. 8 of the status report, GA is critical of the lack of prioritization of the some 200 research items identified in the 1974 report,⁶¹ but these research needs are inherent in both the "Work Breakdown Structure" of the GA semiannual reports and their "HTGR Design Data Needs" packages. At present, there exists only an implied correlation between the work prioritization attributed to the AIPA in formal document submittals and the category definitions used in the actual data needs package. Because nuclear reactor power plants are complicated devices, the objective of the *Planning Guide for HTGR Safety* appears to have been to develop a foundation for a broad base of knowledge of systems, components, and materials performance. That base of knowledge has been evolving for many years independent of the priorities more recently delineated by the probabilistic risk assessment of the AIPA. The results of the AIPA have taken selective advantage of the experience and engineering judgment that defined the initial safety research program and that, in fact, still continues to guide much of that effort.

In summary, the effect of the AIPA on achieving safety in the HTGR design appears to be overstated, and there still exists the more fundamental need to validate plant performance and accident analysis models.

10.5.2 Recommendations for Future Safety Research

Generally, the HTGR safety research effort appears to be generating the types of data necessary for licensing a safe and reliable power plant. However, the overall status of research efforts is not readily discernible from any of GA's reporting schemes such as the semiannual reports on HTGR technology programs⁵⁹ and the Resource Evaluation and Control System (RECS) reports, which are very difficult to decipher. From this study,

it is evident that more emphasis is required on the validation of safety and system design computer codes and simulation models. Full advantage should be taken of Fort St. Vrain for this purpose as well as the use of scale models, laboratory experiments, or other testing facilities from which benchmarks can be obtained to validate the deterministic analysis methods to be applied to licensing the lead plant. As mentioned previously, this type of work is already being performed for mechanisms associated with the worst-case core heatup accident but should be expanded to all probable accidents and plant transients while taking full advantage of the overlap that occurs in protective and safety functions.

In addition, an effort should be made to update the mechanistic models and data used in the AIPA such as the fission product source terms, which have undoubtedly changed since there have been subsequent changes both to fuel design (i.e., the elimination of Biso particles) and to postulated particle failure mechanisms (e.g., palladium attack). Also, since the NRC will most likely require probabilistic risk assessments to supplement other licensing analysis before permitting the construction and operation of a large HTGR lead plant, the failure and event probability data base used in the AIPA should be fully documented and subjected to a genuine peer review that can evaluate the applicability of any data used and, more important, the logic and assumptions behind the engineering judgments being made.

In essence, at this time we believe that little effort should be placed on developing or refining probabilistic risk assessment methodologies but, rather, that more attention should be given to validating both the deterministic simulation models and the component and system reliability data base.

10.6 MODULAR REACTOR EVALUATION STUDIES — P. R. Kasten and B. A. Worley

Information on small, modular HTR designs is being collected for use in evaluating studies of prismatic (HTGR) and pebble-bed reactor (PBR) fueled concepts; information has been received from Germany (KFA and Gesellschaft für Hochtemperaturreaktor), GA, and GE.

The modular concept utilizes eight 250-MW(t) reactor units as individual heat sources. The purpose is to increase total on-line availability and to reduce public risk with acceptable economics. As a part of this study, the use of a PBR core in the modular concept will be compared with the corresponding prismatic high-temperature reactor (HTGR) reference modular concept.

During this reporting period the preliminary core designs were evaluated for the HTGR and PBR. General Atomic has presented the candidate HTGR design,^{62,63} and the reference PBR is herein estimated. Details of the two designs and reference fuel cycle information are listed in Tables 10.4 and 10.5. The parameters in the tables are preliminary and subject to update.

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Table 10.4. High-Temperature Gas-Cooled Reactor reference core physics parameters for modular study

<i>Core design</i>	
Core thermal power, MW(t)	250
Core diameter (av), m	3.49
Core height, m	6.34
Power density (av), MW(t)/m ³	4.12
Number of fuel columns	85
Fuel blocks per column	8
Fuel block configuration	10-row, FSV type ^a
<i>Fuel cycle</i>	
Fuel cycle	LEU ^b /Th
Refueling interval, years	4
Refueling scheme	Batch, entire core
Average capacity factor	0.80
<i>Reflector design</i>	
Top reflector thickness, m	1.0
Side reflector thickness (av), m	1.0
Bottom reflector thickness, m	1.0
<i>Core cooling</i>	
Coolant flow	Upflow
Inlet temperature, °C	425
Outlet temperature, °C	950
<i>Core control</i>	
Control rod configuration	Single rod per control element
Control rod diameter, cm	8.89
Number in core	19
Number in reflector	12

^aFSV, Fort St. Vrain.

^bLEU, low-enriched uranium

Table 10.5. Pebble-Bed Reactor reference core physics parameters for modular study

<i>Core design</i>	
Core thermal power, MW(t)	250
Core diameter (av), m	3.49
Core height, m	6.34
Power density (av), MW(t)/m ³	4.12
Pebble diameter, cm	6.0
Graphite shell thickness, cm	0.5
Pebble volume fraction in core	0.61
<i>Fuel cycle</i>	
Fuel cycle	LEU ^a /Th
Total pebble core-residence time, years	4.0
Number of pebble passes through core	15
Average capacity factor	0.80
<i>Reflector design</i>	
Top reflector thickness, m	1.0
Side reflector thickness, m	1.0
Bottom reflector thickness (av), m	1.0
Gas gap thickness above core (av), m	1.0
<i>Core cooling</i>	
Coolant flow	Upflow
Inlet temperature, °C	TBD ^b
Outlet temperature, °C	TBD
<i>Core control</i>	
Control rod configuration	TBD
Control rod diameter, cm	6.5
Number in core	10
Number in reflector	16

^aLEU, low-enriched uranium.

^bTBD, to be determine.

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