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AEC RESEARCH AND
DEVELOPMENT REPORT

GA-8600

PUBLIC SERVICE COMPANY OF COLORADO
330-MW(E) HIGH-TEMPERATURE GAS-COOLED REACTOR
RESEARCH AND DEVELOPMENT PROGRAM

QUARTERLY PROGRESS REPORT
FOR THE PERIOD ENDING
MARCH 31, 1968

Prepared under
Contract AT(04-3)-633
for the
San Francisco Operations Office
U.S. Atomic Energy Commission

April 30, 1968

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P.O. Box 608, San Diego, California 92112

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GA-8038-April, 1967, through June, 1967
GA-8270-July, 1967, through September, 1967
GA-8420-October, 1967, through December, 1967

INTRODUCTION

The objective of the research and development program work reported here is to develop and verify the information required to design, construct, operate, and maintain the Public Service Company of Colorado power plant as provided in U.S. Atomic Energy Commission Contract AT(04-3)-633.

Part I of this report includes the work described in Appendix B of the contract; this work consists largely of component development and testing, nuclear analysis, and fuel development and testing. Part II covers the work described in Appendix K of the contract on the fuel transfer machine, the series-steam-turbine-driven circulator, the control rod drives, the steam generator, and coated particles.

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PART I

AEC-SUPPORTED RESEARCH AND DEVELOPMENT PROGRAM
FOR THE PUBLIC SERVICE COMPANY OF COLORADO HTGR

Task I
COMPONENT DEVELOPMENT

REACTOR VESSEL

The objective of this subtask is to develop information necessary to develop and specify the design of the Prestressed Concrete Reactor Vessel (PCRVR) for the plant and to provide a technical basis for obtaining necessary approvals of regulatory bodies and insurance groups, by means of conceptual design, analyses, and testing of materials, scale models of the vessel, and full-scale parts of the vessel, such as tendons and penetrations, where necessary.

Model testing will include heating internally to produce appropriate temperature differences across the concrete, test to destruction, long-term tests, measurements of leakage through the concrete resulting from a simulated crack in the liner and tests to demonstrate the adequacy of the double closures on penetrations. Representatives of interested technical societies, regulatory bodies, and insurance groups will be informed of the progress of the programs and encouraged to witness key tests.

PCRVR Model No. 2 Testing

The 585-psig (NWP) sustained pressure test at ambient temperature that was initiated on August 7, 1967, was interrupted after approximately five months in order that partial detensioning tests could be performed on the model. The purpose of the detensioning tests was to evaluate the behavior of the vessel when groups of tendons were fully detensioned and the vessel pressurized to its reference pressure (RP = 705 psig), to provide experimental confirmation of the analyses used in determining the permissible number of tendons that could fail during operation of the Fort St. Vrain PCRVR before the average strain across the wall shifts from compression to tension. The results of the test are discussed in "PCRVR Model No. 2 Design and Analysis."

Similar analyses were made on Model No. 2 to determine the equivalent number and type of tendons that should be detensioned to simulate the postulated Fort St. Vrain PCRVR conditions. Although one Model No. 2 tendon is approximately equal to four Fort St. Vrain PCRVR tendons, the differences in reference pressures (705 psig versus 845 psig), effective prestressing forces, and reinforcing steel quantities necessitated a separate analytical evaluation of Model No. 2. The results of the analyses of Model No. 2 are summarized as follows:

1. Vertical tendons. Of a total of 36 tendons, four adjacent vertical tendons should be fully detensioned (11.1% loss) to approach zero average stress across the wall at RP = 705 psig.

2. Circumferential tendons. One complete band of circumferential tendons consists of two sets of three tendons (180° wrap) spaced over a wall height of 18 in. A minimum of four out of six tendons traverse any 18-in. cross section. One circumferential tendon should be detensioned to achieve an equivalent 25% loss in one band as postulated for the Fort St. Vrain PCR.V.
3. Crosshead tendons. Eight adjacent crosshead tendons on one face of the vessel represent 33% of the total crosshead prestress (eight out of 24 tendons). The four outer crosshead tendons in Model No. 2 were tensioned to approximately twice the force in the four inner crosshead tendons. Because of interferences, the inner crosshead tendons are not readily accessible for detensioning. The four outer crosshead tendons could be detensioned and represent a loss of approximately 22% in the crosshead prestressing.

The tendons were detensioned by rejackng and releasing the residual tendon force. The sequence of detensioning was as follows: four crosshead tendons, one circumferential tendon, and four vertical tendons. The detensioned tendons were in a region of the vessel where strain gages in the concrete and the liner were located. The four outer crosshead tendons were located in the bottom head, the circumferential tendon was approximately at the level of the bottom inner crosshead tendon anchor, and the four adjacent vertical tendons were located between pilasters in the same face where the crosshead tendons were located. Strain and load measurements were made before and after each tendon was detensioned. Sensor measurements were also taken when the vessel was pressurized to RP = 705 psig; this was before detensioning any tendon, and after each group of tendons (i.e., all crosshead tendons) was completely detensioned. After all nine tendons were fully detensioned, the vessel was subjected to a 48-hr pressure hold test at 705 psig. With the completion of the hold test, the vessel was depressurized and the tendons were retensioned to approximately their residual force before detensioning. The 585-psig sustained pressure test at ambient temperature was then resumed and is still in progress.

PCR.V Model No. 2 Design and Analysis

Data obtained during the detensioning tests have not been fully evaluated, but a sampling of concrete and reinforcing steel strains in the vicinity of the detensioned region at various stages of detensioning is given in Table 1.1. No large strain changes were observed. Detensioning of the four vertical tendons caused the major strain increases. Strain reduction was observed in the inner midplane in the circumferential direction (see gages SR 413 and SR 407) after two additional vertical tendons were detensioned, and a strain increase was observed in the outer midplane (see gage SR 823). It appears that the vessel wall was bending over the region where the vertical prestressing had been drastically reduced by detensioning.

Visual examination of the vessel exterior surface during the pressure tests showed no appreciable opening of concrete cracks that had been formed during the previous overpressure tests. Load cells showed little increase in tendon loads during various stages of detensioning. The major load increase

Table 1.1

SAMPLING OF CONCRETE AND REBAR STRAINS IN THE VICINITY OF DETENSIONED
REGION AT VARIOUS STAGES OF DETENSIONING

Gage No.	Location	Gage Orient.	Strain Due to 705-psig Pressure At Stages of Detensioning ^a (μin./in.)				
			1	2	3	4	5
SR 823	Outer midplane at thin-wall sect.	Circumf.	240	243	259	278	284
SR 894	Outer midplane at thin-wall sect.	Vert.	97	91	85	102	124
SR 413	Inner midplane at thin-wall sect.	Circumf.	180	193	219	237	222
SR 895	Adjacent to circumf. tendon detensioned	Vert.	136	124	139	191	252
SR 820	Adjacent to circumf. tendon detensioned	Circumf.	228	230	244	276	296
SR 896	Near detensioned crosshead tendon anchor area	Vert.	27	28	21	26	26
SR 407	Inner midplane at pilaster sect.	Circumf.	215	229	237	274	230
SR 873	Outer midplane at pilaster sect.	Vert.	139	144	137	171	218
SR 881	Inner bottom haunch junction at pilaster sect.	Vert.	167	171	178	203	237
SR 851	Inner bottom haunch junction at pilaster sect.	Vert.	161	167	167	205	253

- ^aStage:
1. Prior to tendon detensioning.
  2. Four tendons detensioned (outer bottom crosshead tendons).
  3. Five tendons detensioned (four crosshead, one circumferential).
  4. Seven tendons detensioned (four crosshead, one circumferential, two vertical).
  5. Nine tendons detensioned (four crosshead, one circumferential, four vertical).

over the residual prestress force was approximately 3600 lb for the circumferential tendons near the midplane when the vessel was pressurized to 705 psig with all nine tendons fully detensioned. This compares with a load increase of about 1400 lb at 705 psig pressure before detensioning. The residual prestressing force of the circumferential tendons at midplane is approximately 120,000 lb.

During the 48-hr hold test at 705 psig with all nine tendons fully detensioned, the vessel behavior was stable with respect to strain changes, load and deflection increases, and pressure fluctuations. During the pressure hold test, load cells indicated random changes with a maximum load increase of about 3%.

Prestressing System

Three different tests (friction, cyclic load, and ultimate load) were performed on one full-size (168-wire) twisted, thermalized-wire, curved tendon. The tests were conducted in a 10-ft-radius tendon tube. For corrosion protection, all tendon wires were coated first with Kephos prior to shipment from England and then with No-OX-500 wax during the tendon fabrication at Western Concrete Structures. No-OX-Id-Cm casing filler was used as a lubricant; it was applied to the tendon tube and the tendon before tendon insertion.

Eight friction determinations were made on the test tendon. The coefficient of friction values obtained ranged between 0.17 and 0.20, with an average value of 0.187. Prior to the ultimate load test the same tendon was subjected to 1000 cycles between loads of 1275 and 1525 kips (i.e., 0.7 GUTS $\pm$ 15,000 psi). There was no sign of failure observed during the complete 1000-cycle test. A maximum tendon load of 2035 kips was obtained during the ultimate tendon load test. This maximum load occurred when the third wire failed. A total of 15 wires were tested to failure. The test results on a thermalized wire tendon were comparable to results obtained on stress-relieved wire tendons.

The two 25-wire thermalized tendon relaxation tests completed more than 2000 hr. Both tendons were initially stressed to 0.7 GUTS. One tendon is at a temperature of 120°F; the other is at ambient temperature. The indicated relaxation losses after 2000 hr are approximately 3% at 120°F and 0.7% at ambient temperature. The relaxation data are shown in Fig. 1.1.

A full-size thermalized wire curved tendon is being fabricated for the long-term relaxation test at 120°F. It will be tested in a semicircular tube with a radius of 16 ft. This tendon will also be utilized to check the long-term stability of high-capacity (1,500,000-lb) vibrating-wire and bonded-strain-gage load cells.

Preparations are being made for a test to demonstrate the adequacy of the PCRV anchor-zone design under long-term loads up to 1900 kips at ambient and 130°F. The tendon anchor test beam has been cast and is under cure prior to posttensioning.

Fig. 1.1--Thermalized wire straight tendon relaxation test data

An evaluation of corrosion protection measures for the prestressing wire is under way. This evaluation is concerned with protecting the wire from rust during ocean transit, including dock-side storage and enclosed storage at the tendon fabrication site, prior to fabrication.

A total of 11 coils (approximately 1000 lb per coil) of 1/4-in.-diameter thermalized wire was shipped from Richard Johnson and Nephew in England and was received at Western Concrete Structures. The shipment was made in three annular metal containers, each with a capacity for four coils of wire. The containers, which are about 7 ft OD and 1.75 ft deep, were constructed in two halves which were sealed with cotton-backed adhesive tape and banded with steel straps.

Some of the coils were coated with Kepros, and others were uncoated. Some of the coated and uncoated coils were unwrapped while others were spiral-wrapped with the following materials:

1. VPI-HB (Hession Burlap) paper with a 4-in. lap, then with 4-in. cotton adhesive tape with a 1-in. lap.
2. Two layers of VPI-HB with a 4-in. lap.
3. One layer of VPI-HB with a 4-in. lap.

The coils were in very good condition on delivery at Western Concrete Structures, none of the wrapping being damaged or punctured.

Visual examination revealed that the coated coils wrapped as in 1 and 2 above were in excellent condition.

Concrete Properties

Phase II Tests. Two sealed concrete specimens (7-in. cubes) were subjected to triaxial loading at room temperature in a hydrostatic pressure chamber. The specimens were loaded at 114 days of age under a hydrostatic pressure of 1600 psi. Limited creep data have been obtained to date, and so no definite trend has been observed.

Premature failures have occurred on the 150°F uniaxial creep cylinder specimens loaded at approximately 100 days of age. The specimens were subjected to sustained stresses varying from 2250 psi to 3960 psi. The cause of premature failure has been attributed to the deterioration of the end caps (sulfur compound) resulting from a combination of exposure to high temperature, corrosion of copper lids, and possible moisture leakage. Fracture of the cylinder specimens occurred at the ends, and close examination of the failed specimens showed the following:

1. The loading surface of the sulfur caps was extremely rough because of uneven corrosion of the copper cover causing nonuniform application of load
2. The concrete surface next to the sulfur cap was unevenly indented revealing local concentration of stresses at the loading ends.

3. Longitudinal cracks were observed within 2 to 3 in. of the cylinder ends, and traces of sulfur were noted in the cracks. It is believed that sulfur in the presence of moisture and high temperature went into solution and percolated through the cracks.

The possibility of sulfur in combination with the environmental conditions causing concrete deterioration through chemical action has not been completely dismissed, although the results of current investigations (X-ray diffraction and fluorescence analyses, and petrographic analysis of samples from fractured specimens) indicate otherwise. Further work is in progress to investigate the possibility of thermal and chemical incompatibility of the different materials used in testing the concrete specimens. Compressive strength tests were conducted on 2-in.-cube specimens obtained from the fractured cylinders and also from an unfractured cylinder. Comparison showed no appreciable difference, except for one sample from a cylinder that prematurely failed at a sustained stress of 3960 psi, the strength of the 2-in. sample being approximately 25% less than the other results.

The first group of cylinders (eight total) originally scheduled for creep loading at 200 days of age was loaded at 230 days. The cylinders were prepared for loading by replacing the sulfur end caps with steel plates and epoxy bonding compound and resealing with copper lids. A different procedure for heating the 150°F creep specimens was adopted in which four specimens were preheated in an oven at a rate not exceeding 15°F per day before the specimens were put in the 150°F water bath. With the previous procedure, the specimens were immediately immersed in the 150°F water bath without being preheated. Heating the specimens at a much slower rate should prevent steep temperature gradients and thus minimize tensile stresses inside the concrete cylinder. The preheated specimens were immediately transferred into the 150°F water bath; two specimens each were subjected to sustained stresses of 2250 psi and 3390 psi. Each of two additional specimens was placed under load at the same stresses in the 74°F water bath.

Creep curves from uniaxial tests of sealed specimens at 74° and 150°F are given in Figs. 1.2 through 1.6. Figures 1.2 and 1.3 show creep curves for specimens loaded at ages of 30 and 100 days, respectively, at room temperature. For clarity, only average lines (two specimens per stress level) are shown; the average curves are obtained as shown in Fig. 1.6. Where the spread between two test results is greater than 50 $\mu\text{in./in.}$, the separate curves are shown as in Fig. 1.3 for $S = 3960$ psi (also in Fig. 1.5). Room-temperature (approximately 74°F) creep exhibits linearity on log-time basis, at least up to an applied stress of 3960 psi. The relationship between room-temperature creep and applied stress is shown in Fig. 1.4, where creep strain measured after 100 days under load is plotted against applied stress for specimens loaded at ages of 30 and 100 days. Creep-stress linearity is observed within the range of applied stresses used, except as noted for the wide spread in results of creep tests at 3960 psi. The effect of age at loading is demonstrated in Fig. 1.4; for the same applied stresses of 2250 psi and 2820 psi, the creep strains of the 30-day-old specimens were larger than those of the 100-day-old specimens by approximately 130 and 280 $\mu\text{in./in.}$, respectively, after 100 days under load. As noted in the figures, the measured creep shown includes autogenous shrinkage. Shrinkage strains are shown in Figs. 1.7 through 1.9.

Fig. 1.2--Uniaxial creep of specimens loaded at 30 days of age at 74°F; autogenous shrinkage is included

Fig. 1.3--Uniaxial creep of specimens loaded at 100 days of age at 74°F; auto-genous shrinkage is included

Fig. 1.4--Applied stress versus uniaxial creep after 100 days under load at 74°F

Fig. 1.5--Uniaxial creep of specimens loaded at 30 days of age at 150°F; autogenous shrinkage is included

Fig. 1.6--Uniaxial creep of cruciform specimens reloaded at 180 days of age to a stress level of 3120 PSI; for previous load history, refer to Figs. 1.19 and 1.20 of GA-8420

Fig. 1.7--Shrinkage of sealed cylinder specimens; all tests are in progress except the one on 20622-A033; gage malfunction occurred after 90 days in 150°F

Fig. 1.8--Shrinkage of sealed cruciform specimens at different temperatures; tests are in progress

Fig. 1.9--Shrinkage of unsealed (in water bath) cruciform specimens at different temperature tests are in progress

Figure 1.5 shows creep curves for specimens heated and loaded at an age of 30 days at 150°F. This figure corresponds to Fig. 1.2 of the room-temperature creep tests. Creep at 150°F shows nonlinear behavior with log-time; in general, the creep curves are S-shaped, with the transition portion occurring between 15 and 60 days after loading. The sharpness of the transition portion of the curve appears to be stress dependent; the transition curve is, however, independent of the time of temperature soaking prior to load application (see Fig. 1.6). The portion of the curve after the transition region appears to be linear with log-time. The curves shown on Fig. 1.5 include autogenous shrinkage; it will be seen later from Figs. 1.7 and 1.8 that the autogenous shrinkage within the first 100 days after heating is large and can be between 200 and 500 $\mu\text{in./in.}$ in strain magnitude.

Two cylinder specimens at 150°F that have been under sustained stress of 2820 psi were unloaded after 210 days (Fig. 1.5). The cylinders were allowed to cool to room temperature and tested to failure under short-term loading. Compressive strength results were 5690 psi (approximately equal to f'_c) and 4360 psi. Examination of the latter test cylinder revealed large voids near the middle region of the specimen, and this particular specimen also produced the larger creep strains of the two specimens at 2820 psi stress as shown in Fig. 1.5.

The maximum range of several vibrating-wire gages was exceeded for specimens under creep tests at 150°F (Fig. 1.5). The strain range of the vibrating-wire gages used in this test program can be as much as 4000 $\mu\text{in./in.}$ in compression, depending on the initial tension of the wire when concrete is cast around the gage. In some instances, precompression of the gage occurred during specimen fabrication and a considerable initial reduction in strain range resulted.

Figure 1.6 shows uniaxial creep curves from cruciform specimens that were reloaded to a stress level of 3120 psi at 180 days of age, both at 74° and 150 F. The previous loading history of the specimens was shown in Figs. 1.19 and 1.20 of the previous quarterly report (GA-8420); the creep curves due to the initial loading of 1585 psi are shown here in Figs. 1.2 and 1.5.

No appreciable difference in creep rates is observed between the two temperatures during the first 15 days after loading. The effect of elevated temperature on creep becomes pronounced after that and corresponds to the initiation of the transition portion of the S-shaped creep curve. A comparison of creep (including autogenous shrinkage) strains after 60 days under load, as taken from Figs. 1.2, 1.5, and 1.6, is given in Table 1.2.

Shrinkage curves from unloaded control cylinders and cruciforms are shown in Figs. 1.7 through 1.9. Figure 1.7 shows shrinkage curves of sealed cylinder specimens at temperature environments of 74° and 150°F. The rate of shrinkage of concrete specimens at 150°F decreases with time, and for the particular concrete tested, the increase in shrinkage strains 100 days after heating becomes small and practically nil (see also Figs. 1.7 and 1.9). Concrete at room temperature showed no appreciable shrinkage during the first 10 days after casting, and after that shrinkage increases linearly with log time. Elevated temperature shrinkage is associated with the regeneration

and acceleration of the concrete hydration process due to heating. It is important to note that concrete shrinkage at 150°F is practically complete 100 days after heating, and no appreciable increase is observed thereafter.

Table 1.2
CREEP STRAIN AFTER 60 DAYS UNDER LOAD

	Creep Strain ^a (μ in./in.)	
	74°F	150°F
Loaded to 1585 psi at 30 days of age	180 (-30)	470 (-256)
Reloaded to 3120 psi at 180 days of age	335 (-15)	475 (0)

^a-Values in parentheses are autogenous shrinkage estimated from Fig. 1.8.

Figures 1.8 and 1.9 show shrinkage curves from sealed and unsealed cruciform specimens in both 74° and 150°F temperature environments. No significant difference between the shrinkage of sealed and unsealed specimens is noted at both temperatures, except for one curve from a T3 gage shown in Fig. 1.9. The elevated temperature shrinkage for cruciforms also exhibit the same tapering off 100 days after heating. Room-temperature shrinkage observed in cruciform specimens was much less than that observed in cylinder specimens (see Figs. 1.7 and 1.8); the restraining effect due to specimen size and shape appears to be the reason for the difference.

Preplaced Aggregate Concrete Mix Test Program. A program of testing was initiated at the University of California at Berkeley through Intrusion-Prepakt, Incorporated of Cleveland, Ohio to finalize the mix design to be used for the preplaced aggregate concrete (PAC). The tests include grout mix proportion and strength determinations (compressive, tensile, E-modulus, and Poisson's ratio) on PAC cylinder specimens. Also included in the program is the determination of heat generation properties of PAC required for calculating the temperatures due to heat of hydration of the PAC during the construction stage.

The grout mix tests have been completed, and a grout consistency of 22-sec (flow factor) has been selected. The two grout proportions that are being investigated in the PAC cylinder strength tests are 9-1-10 and 9-1-8 mix proportions of cement, fly ash, and sand. All test cylinders have been grouted and are scheduled for testing at ages of 7, 28, 90, and 180 days. Average cylinder strength results of the completed tests are given in Table 1.3.

THERMAL BARRIER

The objective of this subtask is to establish the suitability and reliability and predict the maintainability of the thermal barrier materials

Table 1.3
RESULTS OF PAC MIX TESTS

Age at Test (days)	9-1-10 Mix				9-1-8 Mix			
	Compressive Strength (psi)		Tensile Splitting (psi)		Compressive Strength (psi)		Tensile Splitting (psi)	
	Moist Cured	Sealed	Moist Cured	Sealed	Moist Cured	Sealed	Moist Cured	Sealed
7	2690	2740	---	---	2950	2950	---	---
28	5960	5620	610	610	6060	5570	630	610

and design for predicted operating conditions of the plant PCRV by means of analyses, conceptual designs, fabrication development, materials testing, thermal testing, and accelerated life testing where meaningful, on thermal barriers under simulated environmental conditions (in helium at temperatures up to or greater than those predicted in the reactor and, where analyses indicate necessary, at reactor pressure).

Potential advantages of fibrous ceramic blanket insulation have led to a design study of its application to insulating the PCRV liner, instead of multilayer metal thermal barrier.

To support this study, numerous tests have been conducted, since the use of the material in this environment is unique. Most of the data already available were generated for aerospace purposes; conductivity measurements, for example, have been mainly taken in air at reduced pressure. Topics covered by these programs and their significance are given below:

1. Chemistry. Compatibility with other reactor components.
2. Dimensional Stability (Creep and Shrinkage). The ability of the material to maintain a tight seal against the liner over its required life.
3. Depressurization. The ability of the insulation to withstand rapid depressurization of the vessel without damage to either the blankets or the metal attachments and cover plates.
4. Erosion. Erosion resistance of the fibers under the combined influence of mechanical vibration, pressure changes, and aerodynamic effects.
5. Irradiation. The adequacy of the mechanical properties of the fibers after exposure to radiation dosage.
6. Mechanical Properties. The compressive load that is required to achieve desired fit against liner (relates to permeability) versus the amount of mechanical damage to the fibers that can be tolerated (static and cyclic conditions).

7. Permeability. The thermal performance of any insulation system operating in a high-pressure gas environment is strongly dependent upon convection effects. These are related to the freedom with which the gas permeates and flows within the insulation. Data relating to this performance factor were almost nonexistent and were developed by test; it was then used to modify existing thermal conductivity values, measured in air at low pressure.
8. Thermal Cycle Damage. The acceptability of damage that might be caused by expansion and contraction cycles of metal elements of the thermal barrier in contact with fibrous insulation.
9. Vibration. The acceptability of damage to the fibrous material by vibration of metal elements such as cover plates.
10. Water Absorption and Desorption. Should a steam leak occur during operation of the plant, the rate at which the fibrous blankets will absorb (or condense) the water, how much water becomes trapped, and how long will be required to remove that water upon resumption of operations. The effect of this water on the characteristics of the insulation must be determined.

In addition to the above test programs, development of an insulation system design approach has been facilitated by trial installations on a full-scale mockup of portions of the bottom head and side walls of the PCRV. The results of these tests have been generally favorable to the concept of fibrous insulation. The specific advantages that have been revealed during the tests were:

1. The material was found to exhibit lower conductivity in high-pressure helium than could be achieved with metal thermal barrier.
2. A significant part of this improved thermal performance could be assigned to superior resistance to permeation flow in areas of high surface-pressure gradients, i.e., smaller forced convection heat transfer.
3. This superior resistance to permeation flow was an inherent property of the material and, unlike the metallic design, did not require the use of close fitting metallic seals, etc.
4. Since the material was compliant and could be compressed tightly against the liner, a natural capacity to maintain a good interface seal in spite of liner deformations was found. The material was found to comply even with the sharp-edged steps in the liner.
5. Since performance is not dependent upon precision fit-up of the metal parts, these may be fabricated to conventional tolerances. Thus, the cost will be less and the probability of installation defects reduced.

Factors that had been identified as potentially troublesome were also examined in these tests:

1. Chemical purity and compatibility was found to be a problem with many of the candidate materials; however, one candidate (Kaowool) was acceptable.

2. Creep, shrinkage, and similar dimensional stability problems were judged to be within acceptable limits.
3. Rapid rates of depressurization associated with accident conditions (such as rupture of penetration closures) did not cause any significant structural failure or deformation.
4. Erosion tests so far have indicated the effect does not exist. Pressure gradients up to 100% greater than the worst predicted in the reactor have been used in these tests.
5. Tests on Kaowool have shown little or no effect of irradiation on mechanical properties as measured by compression-deflection characteristics (i.e., the deflection under a given load after irradiation is within the scatter band of results on as-received control samples). These results were obtained after dosage equivalent to eight years of reactor service.
6. No thermal cycle damage from expansion-contraction cycles of the adjacent metal parts was observed over approximately twice the number of cycles expected in reactor life.
7. Vibration of cover plates at amplitudes 100 times the expected service conditions did slight damage to properly installed Kaowool. Under these conditions, metal cover plates failed before there was appreciable damage to the Kaowool. Sonic fatigue testing at service conditions plus 10 db produced no detectable damage.
8. The fibrous material was found to have a very large capacity to absorb water. Conditions exist to cause condensation in the insulation during a steam leak accident; however, analysis shows the quantity will be small (mainly because low permeation flow rates limit access), and tests and analysis combined show that this quantity can be removed in a reasonable time by operation of the primary coolant loop at 30% rated flow with the core subcritical. In the worst accident case examined, this dryout phase took approximately one week. Absorption of large quantities of water and subsequent drying has been shown to have no physical effect on Kaowool other than slight compaction under pressure, which had very little effect on the permeation flow resistance of the insulation.

The above review summarizes the major factors covered by test programs on fibrous insulation and the effects of their results on design. A more detailed presentation of results of two of the programs is presented below.

Permeation Flow Tests

At an early stage of the fibrous insulation design study, it was determined from screening tests that the various insulation blankets under consideration varied widely in their ability to resist permeation flow. Since permeation effects strongly influence the conductance of any insulation operating in a high-pressure gas environment, it became necessary to develop detailed knowledge of the factors controlling it. In the screening tests the resistances of various fibrous materials were investigated and shown to be sensitive to (1) average fiber diameter, (2) bulk density, and (3) degree of fit at metal-fiber interfaces.

Factors (1) and (2) are basic to the particular material and, as a result of the screening tests (in combination with reviews of their other properties), it had been determined that Kaowool and Microquartz possessed desirable permeation resistance properties, making them worthy of further consideration. The Microquartz had shown approximately four times the flow resistance of Kaowool at similar densities. (The average fiber diameter of Microquartz is $1\ \mu$, Kaowool $4\ \mu$, but either would be acceptable). Factor (3), however, is affected by more complex variables in which there is interrelation between the material properties, design approach, and system configuration. Further, it was felt that permeation flow at interfaces could be a large percentage of the total and, since it could be affected by so many variables, a more detailed test program was called for.

Using mainly Kaowool and Microquartz as the basic insulation materials, the effect of the following design and material properties variables were investigated:

1. Effect of service temperature on permeability (perhaps as a result of creep or shrinkage).
2. The effect of a severe water leak followed by drying.
3. Best types of splices between insulation blankets.
4. Possibility of adding sheet metal reinforcement between blankets and the permeation penalty associated with various types of reinforcement.
5. The similarities and differences shown by all types of insulation blanket considered for items 1 through 4.

The tests were conducted in the apparatus shown in Fig. 1.10. Figure 1.10 shows N_2 being used, but helium was substituted after the first few tests and was used throughout the tests reported here. All tests were made at room temperature and atmospheric pressure at the outlet. Several values of ΔP were used for each specimen, though when these were found to follow a purely linear relationship, the number of points taken was reduced to two per specimen toward the end of the program. A typical test specimen is shown in place in Fig. 1.11; the top plate, through which the compression is applied to the blankets, is being lowered into place. Figure 1.12 shows how expanded metal mesh is laid between blankets to provide reinforcement and support, particularly for application to vertical surfaces.

Table 1.4 shows a summary of test results; significant conclusions drawn from these results are:

1. The initial advantage that Microquartz enjoys over Kaowool is lost after wetting and drying (flow increases from around 0.3 to 1.3-1.6 SCFM).
2. Thermal aging and wetting-drying cycles have small consistent effects on Kaowool. Thermal aging improves its performance slightly. Wetting and drying cycles degrade it.
3. Normal variations of thickness and density in nominal 1-in.-thick Kaowool affect its permeation flow resistance. A specimen with an

Fig. 1.10--Thermal barrier fibrous specimen flow permeation test rig

Fig. 1.11--Method of installing fibrous insulation blankets in test rig

Fig. 1.12---Expanded metal reinforcement in insulation blanket

Table 1.4

RELATIVE PERMEATION FLOWS WITH FIBROUS INSULATION
($\Delta p = 0.5$ psi)

No.	Condition	Configuration	Flow (SCFM of He)
Kaowool Nominal 8-lb/ft ³			
PKK2-1	As received	5 1-in. blankets, compressed to 3-in. Density 13.8 lb/ft ³	1.05
PKK2-2	Aged at 800°F for 8 hr		0.98
PKK2-3	Immersed in water, dried at 750°F		1.16
PKK2-4	2 layers perforated metal added between blankets		1.10
PKK2-5	Same as -4 except used (1/4 cell) expanded metal		1.18
PKK3-1	As received	5 1-in. blankets, compressed to 3-in. Density 13.25 lb/ft ³	1.33
PKK3-2	As PKK2-2		0.98
PKK3-3	As PKK2-3		1.30
PKK4-1	As received	5 1-in. blankets, compressed to 3-in. Density 15.1 lb/ft ³	0.8
PKK4-2	As PKK2-2		0.8
Kaowool 8-lb/ft ³ , Microquartz 3-lb/ft ³			
PK2-1	As received	4 1-in. blankets, Kaowool plus 1 1-in. blanket, Microquartz compressed to 3-in. Density 11.8 lb/ft ³	1.10
PK2-2	As PKK2-2		1.20
PK2-3	As PKK2-3		1.38
PK2-4	As PKK2-4		1.30
PK2-5	90° splice para. to flow		1.72
PK2-6	90° splice para. to flow		1.88
PK2-7	90° splice para. to flow		1.9
PK3-1	As received	4 1-in. blankets, Kaowool plus 1 1-in. blanket, Microquartz compressed to 3-in. Density 11.8 lb/ft ³	1.14
PK3-2	As received		1.10
PK3-3	As PKK2-2		1.18
PK3-4	As PKK2-3		1.35
PK3-5	45° splice added para. to flow		1.22
PK3-6	45° splice added para. to flow		1.50
Thermoflex 8-lb/ft ³ , Microquartz 3-lb/ft ³			
PT1-2	As received	4 1-in. blankets, Thermoflex plus 1 1-in. blanket, Microquartz compressed to 3-in. Density 12.5 lb/ft ³	1.52
PT1-3	As PKK2-2		1.68
PT1-4	As PKK2-3		1.44
PT2-1	As received	4 1-in. blankets, Thermoflex plus 1 1-in. blanket, Microquartz compressed to 3-in. Density 11.8 lb/ft ³	1.64
PT2-2	As PKK2-2		1.35
PT2-3	As PKK2-3		1.68
Microquartz 3-lb/ft ³			
PM1-1,2	As received	8 1-in. blankets compressed to 3-in. Density 7.9 lb/ft ³	0.30
PM1-3	As PKK2-2		0.52
PM1-4	As PKK2-3		1.32
PM2-1	As received	8 1-in. blankets compressed to 3-in. Density 7.9 lb/ft ³	0.30
PM2-2	As PKK2-3		1.60
PM3-1	As received	8 1-in. blankets compressed to 3-in. Density 7.9 lb/ft ³	0.26
PM3-2	As PKK2-2		0.30

actual density of 15.1 lb/ft³ yielded a flow of 0.8 SCFM versus 1.05 for a density of 13.8 lb/ft³ and 1.33 for a density of 13.25 lb/ft³. A minimum-allowable density requirement will be necessary.

4. Layers of perforated metal, metal mesh, etc., may be used for reinforcement.
5. The effect of 45° splices in the direction of flow is negligible when the insulation materials are properly installed, but 90° splices are a source of severe degradation in insulating value.
6. The flow values established have been factored into performance calculations for areas of insulation subject to the maximum surface pressure gradients expected in service. For a Kaowool insulation system, the flow within the insulation in a local area adjacent to the core support floor would increase the conductivity from 0.3 to 0.4 Btu/ft-°F-hr. This value has been used in designing the insulation system in that area.

Rapid Depressurization Tests

The tests were designed to verify the structural integrity of the thermal barrier, and its installation and to evaluate the desirability or necessity of vent holes (orifices).

Two single-layer fibrous insulation specimens were fabricated and tested. The first consisted of a single layer (1-in. thick, approximately 3 lb/ft³ density) of Microquartz and four layers (1-in. thick, approximately 8 lb/ft³ density) of Kaowool compressed to a 3-in. thickness (with resulting installed density of approximately 12 lb/ft³). The second specimen consisted of eight layers of Microquartz compressed to a 3-in. thickness (installed density of approximately 10 lb/ft³). An all-metal specimen of standard current design was also tested.

The testing was performed in the autoclave (shown schematically in Fig. 1.13) with helium at room temperature. Two depressurization rates were specified for each specimen. Slow depressurization called for a rate of 0.5 psi/sec to simulate the normal maximum operating rate in the reactor, whereas the rapid depressurization required 33 psi/sec to simulate an incredible failure of a refueling penetration closure. An autoclave pressure of 460 to 480 psi was used in all tests. The mass flow of He at 460 psi and room temperature is equivalent to the mass flow of He at 700 psi and 750°F.

The Kaowool-Microquartz composite was subjected to four test runs: (1) slow depressurization with vents open, (2) slow depressurization with vents closed, (3) rapid depressurization with vents open, (4) rapid depressurization with vents closed. In all tests, the initial depressurization rates exceeded the specified values (1.2 and 1.23 psi/sec for tests 1 and 2, and 41 and 42.9 psi/sec for tests 3 and 4). Both slow decompression tests failed to produce cover-plate deformations in excess of the elastic limit of the plate. However, the rapid depressurization produced a small permanent deformation of the 1/4-in. cover plate in both tests (0.025 in. with vents open, 0.61 in. with vents closed) measured at the free edge of the plate as marked by white rectangles in Figs. 1.14 and 1.15.

Fig. 1.13--Thermal barrier depressurization test fibrous and metal specimen

Fig. 1.14--Kaowool depressurization test specimen after exposure to ΔP of 33 psi/sec vents open

Fig. 1.15--Kaowool depressurization test specimen after exposure to ΔP of 33 psi/sec vents closed

In the case of the Microquartz specimen, only two test runs were performed (slow and rapid decompression) with the vents closed in both cases. The tests with the vents open were eliminated because no significant influence of the vents on the results of the previous tests was observed. The slow decompression tests resulted in no permanent deformation of the cover plates, whereas the rapid depressurization produced average permanent deformation of 0.8 in., but no structural failure of any kind.

Posttest examination of both types of fibrous specimen revealed no evidence of fiber erosion as a result of these depressurization rates.

Metal thermal barrier depressurization tests at similar rates produced no permanent deformations in the cover plate whatsoever.

It is concluded that the thermal barrier will suffer no degradation of its functional performance or structural integrity due to the conditions within the scope of the tests conducted. Furthermore, the results indicate that the stud vent holes are unnecessary and can be eliminated.

PRIMARY COOLANT SYSTEM FLOW

The objective of this subtask is to determine the pressure-drop coefficients in the primary coolant system and to verify the distribution, mixing, and stability of the coolant flow in the primary coolant system under normal and emergency cooling conditions of the plant by means of analyses and model test(s) simulating the PCRV cavity and/or pertinent internal structures thereof.

Tests were made with the Region I flow model (shown in Figs. 1.23 and 1.24 of a previous quarterly report, GA-8270) to evaluate the effect of a high gas outlet temperature from a core region on the gas temperature profiles at entry to the reheater and superheater tube bundles. The same data supplied information on the influence of gas overtemperature from a region on the mixed-mean gas temperature into a module, and the heat distribution from a region to several modules (i.e., the source-sink relationship of the flow in Region I).

The total pressure of the air at the inlet to the hot region (delivered by the hot air supply system) was regulated to equal the ambient pressure felt by the cool flow regions, and the flow rate was adjusted to equalize the wall static pressure of other regions in the ring of regions. Thus, the hot region had the same flow dynamic head as other regions in that ring of regions.

Temperature Distribution

For comparison to any prototype conditions and to account for experimental variations of hot stream heating, the measured temperatures in the test planes were converted to nondimensional numbers of the form,

$$\text{Normalized temperature rise} = \frac{T_{\text{measured}} - T_{\text{ambient}}}{T_{\text{hot stream}} - T_{\text{ambient}}} \times 100 ,$$

which is a percentage ratio of overtemperatures and also may be read as the overtemperature (ΔT) at a point in the model caused by a 100°F overtemperature of the particular hot core exit region.

The nondimensional temperature profiles at planes 4.7, 7, 9 of Module No. 40 for simulated full-flow conditions and with hot air introduced to core regions 3, 9, 10, 21, 22, 23, and 24, respectively, are listed in Table 1.5. It is seen that the spread of temperature across planes 7 and 9 are very small in all cases, indicating that bulk mixing must be occurring in the module entry duct. The temperatures recorded were also used to calculate the heat distribution to modules as described below. This permitted a check on the over-all heat balance of the tests.

The worst temperature profile (the largest variation of temperature within a plane) occurred when hot air was introduced to core exit Region 22, which was also the case giving maximum mixed mean gas overtemperature in the test module. The temperature profile at plane 9 shows a further small smoothing effect of the reheater tube bundle, the normalized temperature variation being reduced from 5.6°F in plane 7 to 4.4°F in plane 9.

The next most pronounced profiles at planes 7 and 9 of Module No. 40 occurred when core region 23 was heated, but it must be noted that in the prototype this region has only five fuel columns supplying it. Plots of the temperature profiles at planes 7 and 9 for these worst cases are shown in Figs. 1.16 and 1.19. The isothermal contours can be easily correlated with the relative positions of the hot and cold source regions and the location of the group of core support posts near the entry to the Module No. 40 inlet duct.

This suggests that the bulk mixing is occurring in a symmetrical way, possibly by the succession of diffuser inlet contraction, diffusion, and two concentric sudden expansions of the module entry ducting.

Similar tests were made with two-thirds reactor design gas flow simulated in the model. Results of these tests normalized to 100°F overtemperature of the core regions are presented in Table 1.6. Results again show small variations of temperatures over the inlet plane to the steam generator module. The most severe variation in plane 7 was 5.3°F when core region 22 was flowing hot, and this reduced to 3.5°F in passing through the reheater tube bundle.

A series of temperature streaking tests was made to measure the persistence of hot gas streaks from individual fuel columns and groups of two and three fuel columns. The variation of temperature over a plane in the module was found to be very small. The maximum, minimum, and spread of normalized overtemperatures are listed in Tables 1.7 and 1.8 for a number of test conditions. These tables indicate the very small variation of temperature present in planes 7 and 9, the largest variation being only 3.1°F.

Table 1.5

GAS OVERTEMPERATURE (ΔT) IN MODULE NO. 40 DUE TO 100°F
HOT GAS STREAK FROM CORE REGIONS

Run No.	Design Flow (%)	Core Region	ΔT in Traversed Planes (°F) (High-low Spread)		
			Plane 4.7	Plane 7	Plane 9
80	100	22	32.8	31.9	31.8
			23.5	26.3	27.4
			9.3	5.6	4.4
81	100	23	19.0	17.5	17.6
			13.3	11.8	14.7
			5.7	5.7	2.9
82	100	10	17.6	17.7	17.8
			14.9	13.8	15.9
			2.7	3.9	1.9
83	100	9	16.3	16.6	17.0
			12.3	11.7	14.6
			4.0	4.9	2.4
84	100	3	12.1	N/A	N/A
			10.4	N/A	N/A
			1.7	N/A	N/A
85	100	21	7.8	7.4	7.5
			6.1	6.4	7.2
			1.7	1.0	0.3
86	100	24	4.1	3.6	3.4
			1.9	2.5	3.2
			2.2	0.9	0.2

Table 1.6

GAS OVERTEMPERATURE (ΔT) IN MODULE NO. 40 DUE TO 100°F
HOT GAS STREAK FROM CORE REGIONS

Run No.	Design Flow (%)	Core Region	ΔT in Traversed Planes (°F) (High-low Spread)		
			Plane 4.7	Plane 7	Plane 9
90	67	22	31.1	30.6	30.5
			24.3	25.3	27.0
			6.8	5.3	3.5
117	67	23	20.9	20.7	20.7
			15.1	16.7	16.6
			5.8	4.0	4.1
92	67	10	16.3	16.4	16.2
			14.7	12.9	15.2
			1.6	3.5	1.0
93	67	9	15.5	15.2	15.0
			12.6	12.3	13.2
			2.9	2.9	1.8
94	67	24	3.3	3.5	3.6
			2.5	2.3	2.7
			0.8	1.2	0.9
95	67	21	5.4	5.4	5.6
			4.4	3.7	4.4
			1.0	1.7	1.2
118	67	3	10.8	11.4	11.4
			8.8	9.9	8.8
			2.0	1.5	2.6

Table 1.7

GAS OVERTEMPERATURE IN MODULE (ΔT) DUE TO 100°F
HOT GAS STREAK FROM CORE REGION FUEL COLUMNS

Run No.	Design Flow (%)	Core Region	Fuel Columns Heated	ΔT in Traversed Planes (°F) (Highest-lowest Spread)		
				Plane 4.7	Plane 7	Plane 9
97	100	22	5	4.2	4.5	4.7
				2.5	2.0	3.3
				1.7	2.5	1.4
98	100	22	4,5	8.2	8.0	8.0
				6.2	6.4	6.9
				2.0	1.6	1.1
99	100	22	4,5,6	12.1	11.8	11.8
				9.3	8.7	10.3
				2.8	3.1	1.5
100	100	23	5	4.9	5.2	5.1
				3.4	4.0	4.3
				1.5	1.2	0.8
101	100	23	5,6	6.7	6.5	6.5
				5.0	4.7	5.6
				1.7	1.8	0.9
102	100	23	4,5,6	10.1	9.6	9.6
				7.7	8.4	8.9
				2.4	1.2	0.7

Table 1.8

GAS OVERTEMPERATURE IN MODULE (ΔT) DUE TO 100°F
HOT GAS STREAK FROM CORE REGION FUEL COLUMNS

Run No.	Design Flow (%)	Core Region	Fuel Columns Heated	ΔT in Traversed Planes (°F) (Highest-lowest Spread)		
				Plane 4.7	Plane 7	Plane 9
103	67	23	4,5,6	10.7	10.4	10.4
				8.4	8.4	8.7
				2.3	2.0	1.7
104	67	23	4,5	7.4	7.4	7.1
				6.0	5.7	6.5
				1.4	1.7	0.6
105	67	23	4	3.6	3.8	3.7
				2.6	2.7	2.9
				1.0	1.1	0.8
106	67	22	4,5,6	11.4	11.1	11.0
				7.4	8.8	9.3
				4.0	2.3	1.7
107	67	22	4,5	6.7	6.9	6.8
				3.6	5.3	5.5
				3.1	1.6	1.3
108	67	22	5	5.6	5.4	5.5
				4.3	4.0	4.3
				1.3	1.4	1.2

Fig. 1 16--Overtemperature contours in plane 7 (test 80) for 100°F gas over-temperature from core region 22

Fig. 1.17--Overtemperature contours in plane 9 (test 80) for 100°F gas over-temperature from core region 22

Fig. 1.18--Overtemperature contours in plane 7 (test 81) for 100°F gas over-temperature from core region 23

Fig. 1.19--Overtemperature contours in plane 9 (test 81) for 100°F gas over-temperature from core region 23

It was concluded from the tests that the worst gas temperature profiles in the steam generator modules occur when the temperature of gas from a region in the outer ring adjacent to a module (Region 22 as tested) deviates from an otherwise even core gas exit temperature. However, assuming a 100°F over-temperature of any one region in the core does not create a serious temperature profile in the steam generator module. The test data may be used as guidance to the effect of any present or future core exit gas temperature profile on steam generator inlet temperature profile.

Flow Distribution

In the study of gas flow distribution, hot air was introduced into regions 3, 9, 10, 21, 22, 23, and 24 of the model in turn. The percentage heat flow to the modules in each case was calculated and is listed in Tables 1.9 and 1.10 for design and 67% primary coolant flow, respectively. The percentage of a core region's heat flow to a module is also an indication of the distribution of region mass flow. The flow into Module No. 40 from each core region supplying it is also listed in Tables 1.9 and 1.10 as a percentage of the total module flow.

It can be seen that the heat distribution to the three modules is much the same for either 67% or 100% of design flow. In each case it is seen that Region 22 supplies about 29% of Module 40 flow, and four other regions supply about 16% each. This is compatible with what might be expected from symmetry considerations of the physical relationship of Module 40 and core regions.

The percentage of module flow from a region may also be read as the increase in mixed mean module gas inlet temperature (°F) that would occur if the gas in the region in question was 100°F hotter than that in the remainder of the core regions.

Further tests were made to determine the dependence of the flow distribution patterns on the mass flow rates of the heated (tracer) core regions relative to the average flow from other core regions. Results of these tests for 100% and 67% of design flow are listed in Tables 1.11 and 1.12. Equivalent values for uniform core region distribution are shown for direct comparison.

The mixed mean module gas inlet temperature information is being used in the study of improved methods for predicting source-sink relationships in Region I for all conditions of the core. This has previously been done using potential flow theory that implies an ideal gas in nonviscous irrotational flow.

COMPRESSORS

The objective of this subtask is to establish the suitability and reliability of the compressors and their related operating controls and instrumentation for expected operating conditions in the plant, except radiation, by means of testing and analysis of such items as over-all equipment performance, operational limitations, and maintenance requirements.

Table 1.9
SOURCE-SINK RELATIONSHIPS FROM CORE REGIONS TO MODULES FOR 100% FLOW

Core Region Heated	Percent of Heat to Module 39	Percent of Heat to Module 40	Percent of Heat to Module 41	Percent Heat Balance Error (Heat to Modules - Heat to Hot Region)	Percent of Module 40 Flow
3	10.5	45.5	44.0	-6.7	14.9
9	54.0	48.0	-2.0 ^a	-1.2	15.8
10	6.7	52.9	40.4	+1.5	17.4
21	83.2	19.0	-2.2 ^a	-2.4	6.2
22	18.4	85.0	-3.4 ^a	-5.9	28.0
23	5.4	45.3	49.3	+2.5	14.9
24	4.2	<u>8.6</u>	87.2	+1.5	<u>2.8</u>
		304.3			100.0

^a A negative heat to a module is irrational and must be due to inaccuracies of temperature measurement.

Table 1.10
SOURCE-SINK RELATIONSHIPS FROM CORE REGIONS TO MODULES FOR 67% FLOW

Core Region Heated	Percent of Heat to Module 39	Percent of Heat to Module 40	Percent of Heat to Module 41	Percent Heat Balance Error (Heat to Modules - Heat to Hot Region)	Percent of Module 40 Flow
3	16.3	34.4	49.3	+2.4	12.2
9	53.2	46.8	0	+0.4	16.6
10	6.3	49.3	44.4	+7.9	17.5
21	90.9	14.8	-5.7 ^a	-9.0	5.2
22	18.8	82.1	-0.9 ^a	-2.6	29.0
23	4.4	47.8	47.8	+9.6	16.9
24	3.6	<u>7.3</u>	89.1	+3.9	<u>2.6</u>
		282.5			100.0

^a A negative heat to a module is irrational and must be due to inaccuracies of temperature measurement.

Table 1.11

SOURCE-SINK RELATIONSHIPS FROM CORE REGION TO MODULES FOR 100% FLOW
EFFECT OF CORE REGION MASS FLOW VARIATION

Core Region Heated	Region Flow Relative to Normal	Heat to Module 39 (%)	Heat to Module 40 (%)	Heat to Module 41 (%)	Heat Balance Error (heat to modules - heat to hot region) (%)
22	+7%	22.6	83.0	-5.6 ^a	-11.0
22	-40%	29.4	78.0	-7.4 ^a	-14.0
22	Normal	18.4	85.0	-3.4 ^a	-5.9
23	+9%	-1.4 ^a	52.9	48.5	-9.2
23	-42%	4.0	45.4	50.6	-8.4
23	Normal	5.4	45.3	49.3	+2.5

^aA negative heat to a module is irrational and must be due to inaccuracies of temperature measurement.

Table 1.12

SOURCE-SINK RELATIONSHIPS FROM CORE REGION TO MODULES FOR 67% FLOW
EFFECT OF CORE REGION MASS FLOW VARIATION

Core Region Heated	Region Flow Relative to Normal	Heat to Module 39 (%)	Heat to Module 40 (%)	Heat to Module 41 (%)	Heat Balance Error (heat to modules - heat to hot region) (%)
22	+29%	21.7	7.30	5.3	+1.7
22	-34%	27.1	77.6	-4.7 ^a	-14.7
22	0	18.8	82.1	-0.9 ^a	-2.6
23	+24%	3.6	49.2	47.2	+14.0
23	-38%	4.4	50.0	45.6	-8.2
23	0	4.4	47.8	47.8	+9.6

^aA negative heat to a module is irrational and must be due to inaccuracies of temperature measurement.

The motor-generator set for the purified helium compressor was received and installed by MTI, and preliminary tests on the motor-generator set and the purified helium compressor were completed.

The compressor was run in the test loop using the motor-generator set, and the head-flow characteristic curve for the compressor at 700 psia was determined (Fig. 1.20). The compressor performed satisfactorily, but one of three rectifier units on the motor-generator set burned out and the others were found to be loose.

Operation of the loop at 12.5 psia to determine the head-flow characteristic curve at this pressure level was attempted. The compressor motor failed apparently because of arcing in the stator. The arcing is believed to have been caused by faults in the insulation (possibly damage to the insulation) aggravated by reduced dielectric strength of helium at the low pressure and a high voltage spike on startup.

A new stator is being supplied by the manufacturer, and the following steps are being taken to prevent another failure:

1. Use of a volt trap to reduce the voltage spike.
2. Use of triple-layer magnet wire instead of double-layer.
3. Application of an epoxy banding ring to the ID of the end turns.
4. Machining of rotor near the end rings to remove sharp edges.
5. Use of phase insulation on the end rings.

INSTRUMENTATION AND ELECTRICAL

The objective of this subtask is to develop sensors for measuring core outlet gas temperatures and moisture detectors and electrical penetrations, and to establish their operating characteristics, reliability, and maintainability in the plant by means of analysis and testing in helium, at temperature and, where analysis and/or preliminary tests indicate a necessity, at pressure and other environmental conditions.

Fuel Element Thermometers

Thermal cycling of four thermocouples has been continued throughout the quarter with 8060 cycles accumulated. Routine calibrations show no drift within ± 0.1 mv. Series resistances have not changed, but the shunt resistances of three of the four thermocouples have dropped from an initial value of 300,000 to about 120,000 ohms, the shunt resistance of the fourth remaining essentially unchanged.

Moisture Detectors

A temperature cycling test of the coupling connecting the gaseous-nitrogen cooling coil of the detector to the inlet and outlet coolant lines has been initiated, and a test to evaluate the effectiveness of the filter in preventing rust deposition on the mirror is being planned.

Fig. 1.20--Results of high-pressure test on purified helium compressor

The electronic circuits for the moisture monitor have been packaged into NIM modules and operated as a complete system. The modules are being temperature tested to ensure proper operation over the specified range of environmental temperatures.

AUXILIARY EQUIPMENT AND SPECIAL TOOLS

The objective of this subtask is to establish the suitability of the manipulators and tools, and the reactor viewing device for use in the plant by means of analysis and full-scale testing.

The preliminary specification for the auxiliary equipment and special tools test program was revised to incorporate comments and is being reviewed. The test program now contains a heating test of the television camera section of the reactor viewing device. The heating test will determine the maximum acceptable operating temperature of the camera. The literature and the manufacturer indicate that the camera could be operated at a temperature considerably higher than the manufacturer's standard temperature recommendation. Operating the camera at a higher temperature reduces the cooling requirements considerably.

RESERVE SHUTDOWN SYSTEM

The objective of this subtask is to determine the most suitable means for providing reserve shutdown capability in the plant, and to establish the basic data needed for detailed design of the system.

A test program is under way to establish the fragmentation characteristics of graphite rupture disks of varying thicknesses with various rupture pressures at operating temperature. Previous tests reported have been at room temperature. The results of this testing were generally satisfactory. Figure 1.21 shows a typical graphite disk after rupture along with the reassembled fragments.

An investigation is being made on a "waffled" graphite rupture disk that would fragment into smaller pieces more consistent with the anticipated capacity of the vacuum removal equipment. A manufacturer of graphite disks has performed some tests with fairly promising results, the disks being scored on one side in roughly 1/2- by 1/2-in. segments. A quantity of disks with the waffled design has been ordered for further testing.

ORIFICING MECHANISM

The objective of this subtask is to develop an orifice valve and mechanism for controlling the inlet coolant flow to the reactor core.

The Phase II environmental test was started on the orifice control mechanism and valve. This test is performed concurrently and in the same test facility as the control rod drive program. Approximately 133,300 valve

Fig. 1.21--Typical graphite disk after rupture and reassembled fragments

cycles have been successfully accomplished, which represents 84% of the total test requirements for the system. It is planned, however, to continue the program for the duration of the control rod drive test, in order to obtain wear data on the solid lubricated gears and bearings and valve components.

Task II
NUCLEAR ANALYSIS

FUEL CYCLE ANALYSIS

The objective of this subtask is to establish the fuel management program for the reactor based on analyses of the effects of the loadings in the initial and replacement segments, including recycle segments in an equilibrium core, on local power generation, reactor control, reactor safety, and economics.

Status of Nuclear Design

Work on Reference Design 5 was completed. This iteration on the nuclear design was based on new cross-section data and an axially unzoned core. The axial power distribution with partially inserted control rods proved undesirable.

The next iteration on the nuclear design is now in progress, Reference Design 6. The primary differences from iteration five are the axial zoning in the reference design and the use of an SiC coating on the fissile and fertile particles. The addition of SiC does not have a large effect on the nuclear design.

Three-dimensional flux synthesis methods are being effectively utilized in the analysis of Reference Design 6, as they were in Reference Design 5. Significant advances have been made in the interpretation of three-dimensional flux distribution data in terms of core temperatures.

Effect of SiC on Nuclear Design

Core Reactivity. The TRISO particle design used for Reference Design 6 is shown on Table 2.1. The total silicon load in the core is about 4350 kg. The poisoning effect of this parasitic absorber (about 0.007 Δk) is compensated in the initial core by a reduction in the Th^{232} load of about 500 kg and in the equilibrium cycle by a small increase in the yearly makeup fissile load. The core U^{233} inventory will be about the same, but slightly larger with TRISO particles than with BISO particles because of the larger fission fraction in U^{235} . The increased fissile inventory in the equilibrium cycle means that the power distributions will be improved with the use of SiC, but the improvement is expected to be very small.

The reactivity lifetime is not affected by the use of TRISO particles. The silicon cross sections are well known and will not increase the uncertainty in lifetime estimates. Some adjustment in the burnable poison load may be desirable but this can easily be accommodated.

Temperature Coefficients. The change in the core temperature coefficients with the introduction of SiC is nearly negligible, as shown on Table 2.2.

The coefficients during the initial cycle are less negative (by about $0.05 \times 10^{-5}/^{\circ}\text{C}$) with SiC because of the reduced thorium load. In the equilibrium cycle, the Doppler coefficient is essentially unchanged, but the total coefficient is more negative because of the larger U^{235} inventory.

Table 2.1
TENTATIVE TRISO PARTICLE DESIGN FOR REFERENCE DESIGN 6

	Fissile Particle	Fertile Particle
Kernel diameter, μ	200	400
Buffer coating thickness, μ	50	50
SiC coating thickness, μ	20	20
Isotropic coating thickness, μ	50	60

Table 2.2
EFFECT ON TEMPERATURE COEFFICIENTS, $10^{-5}/^{\circ}\text{C}$
(With Equilibrated Xenon)

	Initial Core		Equilibrium Cycle	
	BISO	TRISO	BISO	TRISO
Doppler Coeff:				
300°K	-10.1	-10.0	-8.8	-8.9
900°K	-5.8	-5.8	-4.5	-4.5
1200°K	-4.4	-4.4	-3.5	-3.5
1800°K	---	---	-2.4	-2.4
2400°K	---	---	-1.85	-1.85
Isothermal Coeff:				
300°K	-10.88	-10.78	-9.3	-9.4
900°K	-5.55	-5.51	-2.8	-2.9
1200°K	-4.05	-4.03	-2.1	-2.2
1800°K	---	---	-2.0	-2.1
2400°K	---	---	-2.2	-2.3

Shutdown Margins. The effect on the rod worth of using TRISO particles is shown on Table 2.3. Minimum shutdown margins occur during the initial cycle. At 300°K, the full shutdown margin is larger with the TRISO particles than with the BISO particles, 0.065 instead of 0.063 $\Delta k/k$. If the most reactive rod pair is removed, the revised shutdown margin is 0.009 $\Delta k/k$, compared with the prior value of 0.008 $\Delta k/k$. The maximum worth of a single rod pair in the cold critical core increases by +0.001 $\Delta k/k$ in the initial cycle (to 0.039 $\Delta k/k$) but remains unchanged in the equilibrium cycle (at 0.033 $\Delta k/k$).

Table 2.3
EFFECT ON CONTROL ROD WORTH AND SHUTDOWN MARGIN

	<u>Change from the BISO Reference Design</u>
Total bank worth, $\Delta k/k$:	
Initial cycle	+0.002
Equilibrium cycle	-0.001
Rod-pair worth, fully rodded core, $\Delta k/k$:	
Initial cycle	+0.001
Equilibrium cycle	Negligible
Minimum shutdown margin, $\Delta k/k$	+0.001 to +0.002

The improved shutdown margin in the initial cycle is due to the larger bank worth, a result of the reduced Th^{232} load and the smaller average graphite density. The slight changes in the temperature coefficient affect the temperature defect, and therefore the shutdown margin, by a negligible amount.

Accident Analysis. The prompt-neutron lifetime and the delayed-neutron fraction are essentially unchanged with the introduction of SiC. The worst reactivity accident remains the rod-pair withdrawal accident (RWA) in the equilibrium cycle. Since temperature coefficients in the equilibrium cycle will be slightly more negative, the consequences of this accident will be less severe with the revised particle design, though only by a small amount.

Although the temperature coefficients in the initial cycle are less negative by about $0.05 \times 10^{-5}/^{\circ}\text{C}$ with SiC, the effect on the RWA is insignificant. The total coefficient is approximately $4 \times 10^{-5}/^{\circ}\text{C}$, a factor of 100 larger than the expected change.

Fuel Cycle Cost. The use of SiC as a coating on the fissile and fertile particles adds 0.03 to 0.04 m/kw-hr to the equilibrium cycle fuel cycle cost, assuming customary use charges. About 50% of the increase is due to increased depletion costs; changes in fabrication costs appear to be minimal. SiC will cause an increase in the reprocessing costs, but again the effect seems to be relatively small.

Specification of Reference Design 6

The analysis of Reference Design 5, which will be described later, indicated that the presence of partially inserted rods adversely influences the axial temperature distribution. Reference Design 6 has a two-zone axial zoning scheme designed to yield an acceptable fuel temperature distribution in the presence of up to six partially inserted rod pairs. An estimate of the zoning has not yet been completed, but an estimate of the over-all core loadings during the first eight years of core life is shown in Table 2.4. These reflect the presence of over 4300 kg of silicon in the core. Near-equilibrium conditions exist after four to five years.

Table 2.4
FUEL INVENTORY AND YEARLY MOVEMENTS

Cycle No.	Time After Startup (years)	Time in Cycle ^a	Thorium Load		Fissile Particle				Fertile Particle				Total Fissile Movements		Core Fissile Loading ^c		
			Movement ^b (kg)	Core ^c Inventory (kg)	U ²³⁵ Moved ^a (kg)	U ²³³ Moved ^a (kg)	Discharge U Enrichment (%)	U ²³² Level In Discharge (ppm)	U ²³⁵ Moved (kg)	U ²³³ Moved (kg)	Discharge U Enrichment (%)	U ²³² Level In Discharge (ppm)	U ²³⁵ (kg)	U ²³³ (kg)	U ²³⁵ (kg)	U ²³³ (kg)	Pu (kg)
1	0	BOC	19,430	19,430	1030										1030	0	0
	2	EOC	3,430	19,000	40	10	77	20	0	50	94	200	40	60	500	290	2
2	2	BOC	2,360	17,930	200								200		660	230	2
	3	EOC	3,210	17,730	50	10	69	40	1	50	91	270	50	60	460	320	3
3	3	BOC	2,360	16,880	220								220		630	260	2
	4	EOC	3,070	16,680	40	20	62	70	1	50	89	340	40	70	440	330	3
4	4	BOC	2,360	15,970	220								220		620	260	2
	5	EOC	2,770	15,780	30	20	56	100	1	50	87	390	30	70	430	320	3
5	5	BOC	2,360	15,370	220								220		620	250	2
	6	EOC	3,300	15,190	30	20	51	130	3	60	85	440	40	80	430	310	3
6	6	BOC	2,960	14,850	220								220		610	230	2
	7	EOC	2,700	14,670	20	20	47	160	2	50	83	470	20	70	420	300	3
7	7	BOC	2,360	14,330	200								200		600	230	2
	8	EOC	2,190	14,150	20	20	50	130	2	30	84	420	20	50	400	290	3
8	8	BOC	2,360	14,320	220								220		600	240	2
	9	EOC	2,190	14,140	20	30	50	130	2	30	84	420	20	60	410	290	3

^a EOC = end of cycle; BOC = beginning of cycle.

^b A charging operation occurs at the beginning of cycle; a discharge occurs at the end of cycle.

^c The core inventory applies to the periods just before and/or just after the reloading operation.

The core loadings by particle type are also shown on Table 2.4. In the equilibrium cycle, the U^{232} contamination is 130 ppm for the discharged fissile particle and 420 ppm for the discharged fertile particle. The uranium fissile enrichments of the spent particles are, on the average, about 50% and 85%, respectively.

The plutonium content of the core is, predictably, nearly negligible. U^{235} formation in the fertile particle is also very small.

Fuel cycle characteristics are shown on Table 2.5. The specific power is about 1000 kw/kg. The conversion ratio varies from 0.6 to 0.7 in the initial cycle to 0.5 to 0.6 in the equilibrium cycle, about the same as in prior iterations on the nuclear design. Fuel exposure and the neutron-flux level for Reference Design 6 are also about the same as before.

Reactivity Effects in Initial Core

The loading estimates presented above reflect a consideration of all of the secondary effects that could affect the initial reactivity. These are shown on Table 2.6. The nonburnable core impurities refer to such nuclides as iron; the principal burnable impurity is boron. Hydrogen initially trapped in the core will gradually be released causing a drop in core reactivity, an effect similar to a fast-saturating permanent fission product. The corrections attributable to Th^{232} and U^{233} cross sections appear necessary in the light of measurements made in the HTGR Critical Assembly program (Ref. 1*).

A minimum excess reactivity of 0.015 Δk is of course a design goal, and not a reactivity defect of the same type as the other items in the table. This table will be modified and expanded as new data on these secondary effects are developed.

Effect of Burnable Poison on Reactivity Lifetime

The time-dependent excess reactivity in the initial cycle has been examined as a function of the burnable poison design. The results indicate:

1. That flexibility in the time-dependent excess reactivity is readily achievable by varying the number of poison rods per element and their diameter, and
2. That the excess reactivity is not too sensitive to uncertainties in the burnable poison self-shielding factor.

Pertinent data on these points are presented below.

Effect of Burnable Poison Design on Excess Reactivity. It is possible to load six burnable poison rods into each fuel element. The rod diameter must be less than about 0.5 in. Within these constraints, sizeable variations in the time-dependent excess reactivity can be achieved in the initial cycle, as shown on Fig. 2.1. Only about two-thirds of the elements in the initial core contain boron. Those scheduled for discharge after two to three years do not contain burnable poison.

The self-shielding factor is inversely proportional to the product of the pin diameter and boron density in the pin. For a given total load, the

*

References will be found at the end of each section.

Table 2.5
FUEL CYCLE CHARACTERISTICS
(Reference Design 6)

	First Cycle ^a		Equilibrium Cycle ^a	
	BOC	EOC	BOC	EOC
Specific power, kw/kg fissile	<820	1070	1010	1200
Conversion ratio	0.61	0.71	0.54	0.61
FIMA in spent fuel, avg	--	--	--	1.24
Mwd/tonne in spent fuel, avg	--	--	--	97,500
Flux, 10^{14} n/cm ² -sec:				
Thermal range, $E < 2.38$ ev	0.48	0.56	0.54	0.63
Fast range, $E > 0.18$ Mev	0.32	0.36	0.32	0.35

^a BOC = beginning of cycle, EOC = end of cycle.

Table 2.6
REACTIVITY EFFECTS, Δk , IN INITIAL CORE
(Reference Design 6)

Core impurities, nonhydrogen:

Burnable impurities	~-0.003
Nonburnable impurities	~-0.002
Correction due to U^{233} cross-section bias	~-0.003
Correction due to Th^{232} cross-section bias	~-0.0001
Th^{232} grain self-shielding	Under study
Hydrogen level in core	~-0.005
Core expansion	~-0.0001
Effect of spatial temp distribution	Under study
Minimum excess reactivity	0.015

Fig. 2.1--Effect of burnable poison design on excess reactivity: initial cycle, two pins per element, constant total boron load

self-shielding factor increases as the diameter increases, permitting a faster boron depletion rate and a larger time-dependent excursion in the excess reactivity. As shown on Fig. 2.1, the reactivity swing with a 0.5-in. pin is about 0.01 Δk larger than with a 0.4-in. pin, a sizeable effect when compared with the maximum desirable excess reactivity of 0.02 to 0.03 Δk (with equilibrated xenon and Pa^{233}).

If six pins instead of two were used in those elements of the initial core that contain burnable poison, the excess reactivity at midcycle, in the analysis shown in Fig. 2.1, would be 0.04 to 0.05, instead of 0.02 to 0.03.

On the basis of this analysis and an examination of power distribution data, a poison design with 0.040-in.-diameter pins in 12 of the central 19 regions and 0.45-in.-diameter pins in 12 of the outer 18 regions was selected for further detailed study in Reference Design 6.

Effect of Self-shielding Factor Uncertainties on Excess Reactivity.
The burnable poison design is based on a calculated self-shielding factor, expressed in the analysis as

$$g(E) = \frac{C}{1 + K N_B \sigma(E) r_0} \quad ,$$

where C and K are carefully chosen constants, r_0 is the radius of the rod, and N_B is the boron density in the rod. The actual self-shielding factor will be somewhat different than the assumed value; the sensitivity of the excess reactivity to uncertainties of this type is shown in Fig. 2.2. The base case, briefly described in the previous paragraph, is shown as Case 1. The self-shielding factor in the thermal-neutron group is about 0.51. Cases 2 and 3 are identical to Case 1, except that the initial self-shielding factor was adjusted to be 0.44 and 0.54, respectively, changes of about 17% and 6%.

Predictably, the beginning-of-cycle (BOC) reactivity is much more sensitive to the self-shielding factor than the end-of-cycle (EOC) reactivity. If the self-shielding factor is actually higher than assumed in the design (i.e., Case 3 versus the Base Case), the initial excess reactivity could be significantly smaller than expected, and the midcycle and EOC reactivities would be higher than expected, but probably not by very much. Reciprocal statements apply if the actual initial self-shielding factor is lower than expected.

The self-shielding of the boron in the initial core is worth about 0.08 Δk ; i.e., if the self-shielding factor were unity, the boron load would be worth about 0.16 instead of 0.08 Δk . After two years of operation, only about 10% of the boron remains, and it is essentially unshielded and worth about 1% Δk . The relative sensitivity of the initial reactivity to uncertainties in the self-shielding factor is thus understandable, and the nuclear design must take a reasonable uncertainty of this type into account.

Another type of uncertainty is shown by Case 4 in Fig. 2.2. The initial self-shielding factor was assumed to be correctly known, but the burnable poison rod was assumed to be situated in a flux peak. Consequently, as burnup occurs the self-shielding factor would approach the value of C (see equation

Fig. 2.2--Effect of uncertainties in burnable poison self-shielding factor on excess reactivity

above) rather than unity; for Case 4, C was assumed to be 1.15. This situation will exist to some extent for many rods, but as shown in Fig. 2.2, the excess reactivity is fortunately very insensitive to the value of C.

FUEL ZONING AND POWER SHAPING

The objective of this subtask is to establish the fuel and burnable poison distribution and the control rod programming based on analyses of the transverse and axial power distribution as a function of fuel and poison loading, burnup, and control rod positions and considerations of fuel temperatures and economics.

Axial Zoning

In the previous quarterly report (GA-8420), comparative data on an axially unzoned core and a two-axial-zone core were presented. In the absence of control rods, the unzoned core appeared to be as attractive as the two-zone core. The axial temperature distribution and the axial graphite gaps were important in this comparison. Studies recently completed show that in the presence of as few as three partially inserted rod pairs, a zoned core is necessary to yield a desirable fuel temperature distribution. These studies are described below.

Unit Cell Studies on an Unzoned Core. A two-dimensional R-Z unit cell consisting of seven refueling regions was developed for this study on axial zoning, as shown in Fig. 2.3. The top half of the core (which is closest to the core inlet) comprises fuel element layers 1, 2, and 3. The central region, in which a rod pair was at times inserted, was assumed to be composed of fresh fuel, whereas the surrounding six regions were assumed to be unrodded and composed of either fresh or depleted fuel. This unit cell approximately models the full core with three partially inserted rod pairs in ring 3 on 120° centers.

Pertinent data for the unzoned core is shown on Table 2.7, where the average power densities in the regions of the model outlined on Fig. 2.3 are tabulated. For the unrodded case, the average power density in the top half of the model is 1.04, in the bottom half, 0.96. This asymmetry is due to the axial temperature distribution, but it also includes the effects of the rod tips located 4 in. above the core in the top reflector.

When one rod pair is inserted half way into the central region, a large power shift occurs toward the bottom. The average power density in the bottom changes from 0.96 to 1.18. The fuel temperatures in the bottom half of the core in this situation are quite high; a significant fraction of the fuel is, on the average, over 2000°F. This can be evaluated by comparing it to data presented in the previous quarterly report. The maximum fuel temperature of an average element in a newly loaded core with several axial fuel zones is at least as high as 1700° to 1750°F. And in the unzoned core in the absence of rods, using detailed data of which the "unrodded" power densities in Table 2.7 are a summary, the peak fuel temperature in an average element is between 1800° and 1850°F. Compared to these values, 2000°F is quite high.

Fig. 2.3--Two dimensional R-Z unit cell model

Table 2.7
GROSS RELATIVE POWER DENSITY IN UNZONED CELL
(Fresh Fuel in all Regions)

Layer No.	Central Column	Central Region	Surrounding Six Regions	Average
Unrodded Condition				
Top Zone:				
1	0.85	0.85	0.85	1.04
2	1.09	1.09	1.09	
3	1.17	1.17	1.17	
Bottom Zone:				
4	1.12	1.12	1.12	0.96
5	0.97	0.97	0.97	
6	0.79	0.79	0.79	
Rod 1/2 In				
Top Zone:				
1	0.26	0.43	0.61	0.82
2	0.36	0.60	0.86	
3	0.51	0.80	1.07	
Bottom Zone:				
4	1.14	1.18	1.24	1.18
5	1.22	1.23	1.23	
6	1.09	1.09	1.09	

On the basis of these data and a flux synthesis study that will be described later, the axially unzoned core was dropped as the reference design.

Unit Cell Studies to Define a New Reference Two-zone Core. A zoned core was also studied using the unit cell model described above. Several zoning schemes were examined, including the following:

1. The bottom layer of fuel elements was heavily loaded with thorium, the other five layers being uniformly loaded.
2. A 2- to 4-in. plug of pure thorium was introduced at the core-bottom reflector interface, the rest of the core being uniformly loaded.
3. The uranium load in the bottom layer of elements was decreased, the other five layers being uniformly loaded.
4. Several more conventional two-zone schemes were studied in which the top zone comprised the top one-sixth of the core, the top one-third of the core, or the top one-half of the core.

The two-zone core with 50% of the core in each zone was most promising, and the pertinent results are described below.

The axial zoning can be characterized by the power distribution of an unrodded core. The exact loadings that yield such a distribution are for evaluation purposes irrelevant, provided, of course, that there is sufficient flexibility from a metallurgical standpoint in the allowable core fuel loadings.

A two-zone axial distribution that has been the basis of a new reference design is shown on Tables 2.8 and 2.9. In the first table, the data are representative of the unit cell whose seven regions are made up of fresh fuel. In the unrodded condition, the average power densities in the top and bottom zones are 1.47 and 0.53, respectively, a sizeable difference. With one rod pair inserted half way into the central region, the zone power densities, top and bottom, are 1.34 and 0.66, respectively.

If an estimate is made of the manner in which this fuel is depleted, and uranium and thorium loadings characteristic of three-year-old fuel are placed in the six outer regions of the model, the data on Table 2.9 are obtained. This situation is similar to that which would exist after the first, second, etc. reloads. In this case the unrodded power split between zones is 1.24 versus 0.76, whereas with the central rod pair inserted half way, it is 0.96 versus 1.04. This latter power split, with slightly more than half the power in the bottom zone, is believed to be acceptable, though more detailed full core studies are necessary for confirmation. These studies are in progress.

Importance of Burnable Poison. The influence of burnable poison on the time-dependent axial power distributions is significant. Rough survey calculations indicate that a favorable distribution can be maintained with the use of axially unzoned lumped burnable poison, though for reasonably zoned burnable poison designs the axial swings in power density are unacceptable. On the basis of this study, future work will focus first on the use of axially unzoned lumped burnable poison.

Table 2.8
GROSS RELATIVE POWER DENSITY IN ZONED CORE
(Fresh Fuel in all Regions)

Layer No.	Central Column	Central Region	Surrounding Six Regions	Average
Unrodded Condition				
Top Zone:				
1	1.40	1.40	1.40	1.47
2	1.61	1.61	1.61	
3	1.41	1.41	1.41	
Bottom Zone:				
4	0.81	0.81	0.81	0.53
5	0.48	0.48	0.48	
6	0.30	0.30	0.30	
Rod 1/2 In				
Top Zone:				
1	0.54	0.89	1.26	1.34
2	0.64	1.05	1.50	
3	0.66	1.03	1.42	
Bottom Zone:				
4	0.82	0.85	0.92	0.66
5	0.63	0.63	0.64	
6	0.44	0.44	0.44	

Table 2.9
GROSS RELATIVE POWER DENSITY IN ZONED CORE
(Fresh Fuel in Central Region, Average Depleted Three-year-old
Fuel in Surrounding Six Regions)

Layer No.	Central Column	Central Region	Surrounding Six Regions	Average
Unrodded Condition				
Top Zone:				
1	1.38	1.38	1.07	1.24
2	1.67	1.67	1.27	
3	1.62	1.62	1.23	
Bottom Zone:				
4	1.13	1.14	0.91	0.76
5	0.88	0.88	0.74	
6	0.65	0.66	0.56	
Rod 1/2 In				
Top Zone:				
1	0.41	0.68	0.76	0.96
2	0.53	0.89	0.99	
3	0.70	1.10	1.15	
Bottom Zone:				
4	1.21	1.27	1.08	1.04
5	1.26	1.27	1.07	
6	1.06	1.07	0.91	

Flux Synthesis Lifetime Study of Reference Design 5, An Axially Unzoned Core

Reference Design 5 was an axially unzoned core using BISO-coated particles as the fuel. The reactivity lifetime characteristics of this core were examined using single-channel flux-synthesis techniques. The principal result of the study was to confirm prior data that indicated that axial zoning was necessary to give desirable axial power distributions in the presence of partially inserted control rods. Other conclusions were:

1. Simple rod programming schemes can probably be found which, with suitable radial zoning, will yield region power factors considerably below the design value of 1.83.
2. The time-dependent excess reactivity with equilibrium xenon and Pa^{233} is slowly varying.
3. The most limiting power distributions occur during the first few days of the cycle.

The general method used in the flux synthesis study is outlined below. Detailed results are then presented.

Single-channel flux synthesis has been extensively used in nuclear design efforts (Ref. 2). In the present application the transverse calculations at different axial elevations were done using a two-dimensional four-group triangular-mesh code. Flux- and volume-averaged cross sections and radial bucklings from the transverse calculations were then fed into a one-dimensional diffusion theory code, FEVER (see Ref. 3), the output of which was the reactor multiplication constant and the average axial power distribution.

In the Reference Design 5 analysis, two axial transverse cuts were used, corresponding to the top and bottom halves of the core. Except for the presence or absence of control rods, the cuts differed only in the temperature used in determining the applicable cross sections, 880° and 1088°K for the top and bottom cuts, respectively.

A great simplification was realized by allowing control rods to be located at only one of three possible positions; fully in, fully out, or 50% inserted. The reactor multiplication was, as a result, usually slightly different from unity. This imprecision does not affect the principal conclusions, however. Except for the central rod pair, rod pairs were moved in symmetric groups of three, there being 12 groups of three rod pairs in the reactor.

The depletion calculations were carried out on the basis of the top-to-bottom power split determined in the FEVER calculations.

The numerical results of this study are summarized on Table 2.10. The core reactivity, the output of the synthesizing FEVER calculation, sometimes differed from unity by a larger amount than would normally be desired. One goal of this study, however, was to examine the effects of partially inserted control rods on the axial power distributions, and the reactivity defect just mentioned, especially during the initial cycle, was usually a consequence of this motivation.

Table 2.10
SYNTHESIS STUDY OF REACTIVITY LIFETIME
(Reference Design 5)

Time In Life EFPD ^a	No. of Rod Pairs Inserted		K _{eff}			Power Fraction (%)		Comments
			Top Cut	Bottom Cut	Net Core (Synthesis Calculation with FEVER)	Top Half	Bottom Half	
	Top Half	Bottom Half						
Initial:								
0	13	10	1.013	1.006	1.014	47	53	No Xe
2	7	4	1.003	1.003	1.008	44	56	Equilib. Xe
5	7	4	1.000	0.999	1.005	44	56	Equilib. Sm ¹⁴⁹
10	4	4	1.013	0.996	1.009	52	48	
50	4	4	1.000	0.989	0.998	49	51	Equilib. Pa ²³³
104	4	4	1.001	0.988	0.998	50	50	
207	7	4	0.990	0.996	0.997	41	59	
311	7	4	1.000	0.999	1.002	45	55	
414	7	4	1.000	1.003	1.004	44	56	
518	7	4	0.999	0.998	1.000	46	54	
621	4.	4	1.004	0.997	1.002	48	52	End of initial cycle
First Reload:								
0	13	10	1.015	1.0075	1.016	47	53	No Xe, Pa ²³³ partially decayed
2	7	7	1.011	0.995	1.007	51	49	
5	7	7	1.010	0.993	1.006	51	49	
10	7	7	1.008	0.993	1.004	51	49	
50	7	7	1.002	0.987	0.998	51	49	
104	7	7	0.999	0.984	0.995	51	49	
207	7	7	0.996	0.981	0.991	51	49	
311	7	7	0.991	0.977	0.987	51	49	

^a
EFPD = effective full-power days.

The calculated excess reactivity at the end of the initial cycle is about 0.015 Δk , the equivalent of four rod pairs. This, as mentioned earlier, will provide a hedge against an adverse combination of uncertainties affecting reactivity.

The perturbations due to equilibrating xenon and Pa^{233} are evident. The subsequent reactivity rise in the initial cycle due to boron depletion is also apparent. For most of the cycle, reactivity changes are quite small, however.

In the presence of three partially inserted rod pairs, about 55% of the power is produced in the bottom of the core. This is equivalent to a relative power density of 1.10. These results are consistent with the results of unit cell studies described earlier. If six rod pairs were at intermediate positions, the power density in the bottom half of the core would be much higher. At this state of the nuclear design, a restriction on the number of partially inserted rod pairs to three would be undesirable.

The rod withdrawal pattern used in this study, which led to radial power distributions that were well below the design value of 1.83, is shown in Table 2.11. This pattern is different from patterns used in the past, but it has been shown to offer advantages in power distribution over past patterns.

Table 2.11
ROD WITHDRAWAL PATTERN USED IN REFERENCE DESIGN 5
FLUX SYNTHESIS STUDY

Group No. ^a	Worth of Rod Group (Δk)
2B	(b)
4C	(b)
3A	(b)
4E	(b)
3C	(b)
2A	(b)
4A	(b)
3D	0.0288
4B	0.0090
4D	0.0079
3B	0.0153
4F	0.0144
1	0.0030

^aSee "Analytical Studies" in this task for a definition of the group number.

^bNot calculated in this study.

The most limiting radial distributions observed in this study occurred early in both the initial cycle and the second cycle, before xenon and Pa^{233} had built up, at which time 13 rod pairs were fully inserted in the top half of the core and 10 rod pairs in the bottom half. Pertinent data are shown in Figs. 2.4 and 2.5. The highest factor in the initial cycle was 1.67; during the remainder of the two-year cycle, the radial distribution was even more favorable. The situation in the second cycle is similarly favorable.

KINETIC ANALYSIS

The objective of this subtask is to predict the required strength of the control system, the control rod worth, the magnitude of the temperature coefficients, and the kinetic behavior of the core including insertions of reactivity and spatial xenon stability by means of analyses under normal operating and accident conditions.

Sensitivity of Rod Worth to Graphite Density

The rod bank worth has been found to be surprisingly sensitive to the graphite density. A 1% increase in graphite density will result in a decrease in the total worth of the rod bank of 0.0011, when the worth is measured in Δk units, or 0.0023, when the worth is measured in $\Delta \rho$ units. The graphite density assumed in the nuclear design calculations is 1.72 g/cm³. The graphite specification places a lower limit of 1.70 g/cm³ on the acceptable graphite density. This specification is under review.

Sensitivity of Rod Withdrawal Accident to Prompt-neutron Lifetime

The prompt-neutron lifetime of the most recent reference designs is significantly larger than the value assumed in the hazards analyses. The value used in the Preliminary Safeguards Analysis Report for the equilibrium core was 2.6×10^{-4} sec; a more recent estimate of the neutron lifetime is 4.3×10^{-4} sec. The accident that is most sensitive to the neutron lifetime is the rod-pair withdrawal accident starting from source power levels, i.e., the startup accident. The data presented in Table 2.12 show this sensitivity to be very small. The longer neutron lifetime results in a somewhat slower transient, but the effect on the fuel temperature is insignificant.

Rod Worth in an Axially Zoned Core

The rod worth as a function of axial position is dependent, of course, on the axial zoning scheme. Some illustrative data obtained from the seven-region unit cell studies described earlier are shown on Table 2.13.

The rod worth for an insertion of 50% can be correlated with the power density in the top half of the core, as shown in Fig. 2.6. For example, in the two-zone core with the top zone comprising the upper two layers of fuel elements, the power density is very high (1.60) in the top half of the core. As a result, about 90% of the rod worth has been realized by the time it is inserted half way.

Fig. 2.4--Gross radial factor, beginning of initial cycle

Fig. 2.5--Gross radial factor, beginning of second cycle

Table 2.12
SENSITIVITY OF SOURCE POWER ROD-PAIR WITHDRAWAL ACCIDENT
TO THE PROMPT-NEUTRON LIFETIME

	PSAR Analysis	Most Recent Analysis
Prompt neutron lifetime, sec	2.6×10^{-4}	4.3×10^{-4}
β_{eff}	0.0046	0.0046
Rod worth, Δk	0.033	0.033
Initial power level, Mw(t)	10^{-6}	10^{-6}
Rod withdrawal time, sec	180	180
Scram signal	Period	Period
Time scram signal occurs, sec	23	23
Scram reactivity, Δk	0.03	0.03
Peak power, Mw(t)	1600	1720
Time of peak power, sec	34.1	35.8
Peak fuel temperature rise, °C	166	166

Reactivity Worth of Steam in the Core

The reactivity worth of steam in the core has been reevaluated. It appears that an upper limit for the reactivity addition is about $2.2 \times 10^{-5} \Delta\rho/\text{lb H}_2\text{O}$ in the core. The shutdown margin is reduced at a rate of about $2.8 \times 10^{-5} \Delta\rho/\text{lb H}_2\text{O}$ because of the additional reduction in control rod worth. Although the reactivity coefficient is larger than that previously calculated, the total possible reactivity insertion by steam inleakage is still much less than that added in a rod withdrawal accident.

The addition of steam to the core affects the reactivity in several ways:

1. The resonance escape probability increases, since neutrons can bypass the thorium resonances by scattering with hydrogen atoms.
2. The undermoderated system becomes more thermalized. Only for very large amounts of steam in the core ($>16,000$ lb of H_2O in the core) does the absorption in hydrogen overcompensate the thermalization effect.
3. The reduced average core migration area reduces the core leakage.
4. The reduced migration area also decreases the control rod worth.

The reactivity effects of adding various amount of H_2O to the core were determined using a core composition characteristic of the average initial core after one year of operation. The worth of water in subsequent cycles will be less than in the initial cycle because of the reduced Th^{232} load in later cycles. A summary of the results is given in Fig. 2.7. For example,

Table 2.13
 ROD WORTHS IN AN AXIALLY ZONED CORE
 (Seven Region R-Z Unit Cell Calculations)

	Zoning Scheme ^a				
	1/3 / 2/3 Fresh	1/3 / 2/3 Depleted	1/2 / 1/2 Fresh	1/2 / 1/2 Depleted	Uniform Fresh
Power density:					
Layer 1 (top of core)	1.87	1.35	1.40	1.07	0.85
Layer 2	1.89	1.50	1.61	1.27	1.09
Layer 3	1.02	1.04	1.41	1.23	1.17
Layer 4	0.62	0.86	0.81	0.91	1.12
Layer 5	0.37	0.71	0.48	0.74	0.97
Layer 6 (bottom of core)	0.23	0.55	0.30	0.56	0.79
Total rod worth, Δk	0.0205	0.0273	0.0203	0.0253	0.0200
Fraction of total worth obtained when rod is inserted:					
1/3 distance	---	---	---	0.36	0.25
1/2 distance	0.90	0.60	0.81	0.59	0.45
2/3 distance	---	---	---	0.79	---

^a"1/3 / 2/3" refers to the core volume fraction in the top and bottom zones, respectively; "Fresh" refers to the use of fresh fuel in all regions; "Depleted" refers to the use of fresh fuel in the central region and three-year-old fuel in the surrounding six regions.

Fig. 2.6--Correlation between rod worth and axial power distribution; seven-region R-Z unit cell calculations

Fig. 2.7--Reactivity effects due to water addition to core

the addition of 500 lb of H₂O to the core will increase core reactivity by about 0.01 Δk and reduce the total rod bank worth by less than 0.005 Δk . Since the core void volume is only 3% of the primary system volume, over 16,000 lb of H₂O must enter the PCRV to affect core reactivity by 0.01 Δk .

ANALYTICAL STUDIES

The objective of this subtask is to provide support to other tasks of the program by means of analyses of such items as neutron and flux distributions, nuclear safety of auxiliary equipment, loadings and nuclear properties of irradiation experiments, defining of the need for physics measurements during reactor startup, and evaluating the core response to reserve shutdown system operation.

Rod Group Numbering Scheme

A rod group numbering scheme has been established to facilitate the definition of rod programming schemes. The numbering scheme is shown on Fig. 2.8 and Table 2.14. The number refers to the ring in which the rod pair is situated, and the letter refers to a specific group of three rod pairs within a ring.

Table 2.14
ROD GROUP IDENTIFICATION

<u>Group No.</u>	<u>Rod Pair Nos. In Group</u>
1	1
2A	2,4,6
2B	3,5,7
3A	8,12,16
3B	9,13,17
3C	10,14,18
3D	11,15,19
4A	20,26,32
4B	21,27,33
4C	22,28,34
4D	23,29,35
4E	24,30,36
4F	25,31,37

Data Analysis and Evaluation

The development and use of flux-synthesis methods has greatly aided data analysis and evaluation. Since coolant orificing is used to control outlet gas temperatures, it is possible to experience high fuel temperatures in

Fig. 2 8--Standard identification of rod groups

regions with relatively low power densities. Hence, to determine the spatial dependence of the fuel temperatures, the power-flow characteristics of virtually every element in the core must be considered. The required data processing job is obviously very large.

The recent use of flux-synthesis methods has made it possible to determine automatically and to store on a magnetic tape or punched cards the power density in each element of the core. This is now done. The data can then be processed to determine the power density for each refueling region and fed into a thermal hydraulic code, COPE (Ref. 4), the output of which is the flow in each region necessary to give a uniform outlet gas temperature (i.e., the gross flow conditions under the assumption of perfect orificing are determined). The 37 core regions are then automatically examined with COPE, one at a time, to determine the flow in each column of the region and the average axial fuel temperature in each column of the region. The resulting data are then automatically processed yielding a graph of the fraction of the fuel volume above some temperature versus temperature.

This procedure was used successfully in evaluating data from the flux synthesis life study described earlier. A further extension of the method, now under development, will yield data on the average fuel temperatures in many subsections of each column, thereby giving a much more detailed picture of the fuel temperature distribution.

References

1. HTGR Base Program Quarterly Progress Report for the Period Ending February 29, 1968, USAEC report GA-8530, Gulf General Atomic, to be published.
2. Naval Reactors Handbook, Vol. 1, 1964.
3. Todt, F., RELOAD FEVER, A One-dimensional Few-group Depletion Program for Reactor Fuel Cycle Analysis, General Dynamics, General Atomic Division, report GA-6612, September 28, 1965.
4. Katz, R., Core Performance Code, COPE, USAEC report GAMD-1561 (Rev.), General Dynamics, General Atomic Division, March 21, 1963.

Task III
FUEL DEVELOPMENT

FUEL ELEMENT DESIGN

The objective of this subtask is to develop the design of the fuel elements to be used in the plant by means of mechanical and flow testing, analysis of temperatures under normal and abnormal conditions, analyses of stresses, including effects of temperature, irradiation, and creep, and analysis of fission-product concentrations in the reactor system.

Mechanical Design

Dimensions and tolerances for the graphite fuel element blocks have been established, and the fabrication drawings for the machined blocks are in progress. The performance specification for the fuel element and replaceable reflector graphite is being reviewed before final issue. The fuel element graphite drawings and the machining specification are nearly complete.

The material specification for thorium dioxide powder for the fabrication of coated fuel particles was issued.

Thermal Design

Fuel Temperature Analysis. Calculations of temperature in the high-powered region of the core have been made using an improved model with increased mesh points for input to the RAT-2D code. The code describes a symmetric element of the core described in an earlier quarterly report (GA-7314). The purpose of these calculations was to assess the effects of heat generation in the moderator due to gamma ray absorption, the effect of fuel rod radius, fuel rod thermal conductivity, and the effect of a small annular gap between the fuel rod and the moderator on the temperature distributions in the core. For use in the core performance code COPE, three "equivalent conductances" were developed from the RAT-2D results.

In the calculation, the assumption was made that the power generated in the fuel rod did not vary with rod diameter; thus, the larger rods operated at a lower power density than the smaller rods.

The equivalent conductances that are used in the COPE code are defined as:

$$EK1 = \frac{q'}{T_{fmin} - T_w} ,$$

$$EK2 = \frac{q'}{T_{favg} - T_w} ,$$

and

$$EK3 = \frac{q'}{T_{fmax} - T_w} ,$$

where q' is the heat flux at the coolant hole wall; the quantities T_{fmin} , T_w , and T_{fmax} are, respectively, the average temperatures at the outer surface of the fuel, the coolant hole wall, and the fuel centerline; and T_{favg} is the volume-averaged fuel temperature.

Since the temperatures vary with angular position in the symmetry unit, the temperatures T_{fmin} , T_w , and T_{fmax} are defined as temperatures averaged with respect to the surface indicated by their subscript.

Table 3.1 gives the values calculated for several conditions. Cases 1 through 7 include the predicted distribution of heat generation between fuel and moderator; cases 8 through 14 assume all heat is generated in the fuel rods only.

The fuel diameter does not have an important effect on temperature or equivalent conductances in the narrow range of PSC fuel diameters. Equivalent conductances were found to be about 8% higher when 10% of the heat is generated in the moderator than when all heat is generated in the fuel rods. If a maximum radial gap of 0.0045 in. between the fuel rod and graphite moderator exists, the maximum temperature is increased 120° to 140°F compared with no radial gap for the same conditions and with the same fuel rod conductivity.

The equivalent conductances were evaluated by an independent analytical method to check the accuracy of the improved RAT-2D calculations for the conditions of no annular gap around the fuel and no heat generation in the moderator. Values of EK1, EK2, and EK3 shown in cases 8 and 11 of Table 3.1 showed good agreement with the results of the independent analysis. The RAT-2D results are, respectively, 1% larger, 0.8% larger, and 1.1% smaller than those evaluated by the independent method.

Cases 7 and 14 in Table 3.1 show temperature and equivalent conductances in the highest powered fuel rod in the core where the radial peaking factor is 2.35 and the axial peaking factor 1.42. Case 14 represents the conservative method that has previously been used to calculate the local maximum fuel temperature. Here the fuel thermal conductivity used is that measured for loose beds of fuel particles, and all heat is generated in the fuel rod. Case 7 of Table 3.1 represents the realistic estimate of maximum temperature where measured fuel rod thermal conductivity is used. The conservative and realistic estimates of maximum temperature differ by 420°F.

Table 3.1
TEMPERATURES AND EQUIVALENT CONDUCTANCES CALCULATED FOR HIGHEST POWER REGION

Case No.	d_f (in.)	k_f (Btu/hr-ft-°F)	Gap Width (in.)	q_f (Btu/hr-ft ³)	q_g (Btu/hr-ft ³)	T_{fmax} (°F)	T_{favg} (°F)	T_{fmin} (°F)	T_w (°F)	EK1	EK2	EK3
										Equivalent Conductance (Btu/hr-ft ² -°F)		
1.	0.500	1.2	0	7.50×10^6	0.422×10^6	2285	1936	1599	1476	1121	300	170
2.	0.491	1.2	0.0045	7.78×10^6	0.422×10^6	2411	2062	1725	1476	554	235	148
3.	0.491	2.7	0.0045	7.78×10^6	0.422×10^6	2034	1874	1725	1476	554	347	247
4.	0.5469	1.2	0	6.29×10^6	0.422×10^6	2274	1920	1582	1468	1202	303	170
5.	0.5379	1.2	0.0045	6.50×10^6	0.422×10^6	2392	2026	1688	1475	643	249	149
6.	0.5379	2.7	0.0045	6.50×10^6	0.422×10^6	2013	1837	1688	1476	646	379	255
7.	0.491	2.7	0.0045	9.991×10^6	0.5419×10^6	2281	2081	1887	1571	579	359	258
8.	0.500	1.2	0	8.24×10^6	0	2355	1971	1601	1475	1090	277	156
9.	0.491	1.2	0.0045	8.547×10^6	0	2493	2111	1740	1473	514	215	135
10.	0.491	2.7	0.0045	8.547×10^6	0	2079	1906	1740	1473	514	317	227
11.	0.5469	1.2	0	6.889×10^6	0	2342	1955	1585	1468	1169	282	156
12.	0.5379	1.2	0.0045	7.121×10^6	0	2473	2065	1694	1476	630	233	138
13.	0.5379	2.7	0.0045	7.121×10^6	0	2058	1857	1694	1476	630	361	236
14.	0.500	1.2	0	10.583×10^6	0	2701	2212	1734	1571	1081	275	156

NOTE: Cases 1-7, with moderator heating included. Cases 8-14, no heat generated in moderator. $h = 375$ Btu/hr-ft²-°F. $T_b = 1148^\circ\text{F}$, bulk coolant temperature at axial location of maximum temperature.

Crossflow Analysis. In the previous quarter, results were reported on the effects of crossflow on the ability to control core outlet temperature uniformity (in order to provide equal gas temperatures to the steam generator module inlets). These results were obtained for a fixed core geometry with respect to clearances between the fuel element columns and gaps at the fuel element interfaces. Since both of these parameters have a direct influence on the magnitude of crossflow, additional calculations were made to investigate the effect of uncertainties in the gap characteristics. The range of uncertainties investigated was a variation of 16 times greater to 16 times smaller on the individual crossflow loss coefficients, and a factor of 1.2 times greater bypass flow area (the bypass flow is defined as the flow in the vertical gaps between fuel element columns). It may be noted that in a simple flow channel, the flow rate is inversely proportional to the square root of the loss coefficient and directly proportional to the area.

The results of introducing the above parameters into the crossflow analysis are summarized in Table 3.2. The results show the net deviation in region outlet temperature from the core mixed mean for two regions, the maximum power region (Region 24), and the minimum power region (Region 35). In all cases, the effective fully closed orifice loss coefficient used was set at 300. However, the fully open orifice loss coefficient used was the calculated value that has recently been found to be approximately a factor of two higher than the value determined from the full-scale orifice test data. The estimated effect of the increase in fully open loss coefficient will be to increase the exit coolant temperature from the maximum power region by about 100°F to approximately +125°F. Little effect is estimated on the low power region coolant exit temperature.

Table 3.2
EFFECT OF UNCERTAINTIES IN THE CROSSFLOW ANALYSIS ON
PREDICTED CORE REGION OUTLET TEMPERATURE

Uncertainty Parameter	Deviation in Region Outlet Temp from Core Average (°F)	
	Region 24 Max. power; orifice fully open	Region 35 Min. power; orifice fully closed (k=300)
Nominal crossflow loss coeff. x 16	-5	-106
Nominal crossflow loss coeff. x 4	+6	-132
Nominal case	+24	-188
Nominal crossflow loss coeff. ÷ 4	+46	-212
Nominal crossflow loss coeff. ÷ 16	+62	-216
External bypass flow area increased by factor 1.20	+28	-192

From these results, it can be seen that very large changes in crossflow loss coefficient have only a modest effect on the region outlet temperatures. Decreasing the loss coefficients by a factor of 16 from the nominal case results in an increase in Region 24 outlet temperature from 24°F above the core average to 62°F above and a decrease in Region 35 outlet temperature from 188°F below the core average to 216°F below. Similarly, increasing the bypass flow area by 20% has a much smaller effect on the region outlet temperatures. Apparently the secondary flows in the core are so interconnected that they are a function of a large number of variables and are not strong functions of any single parameter.

The expected uncertainty in the crossflow loss coefficients can be estimated from results of the crossflow loss coefficient test. This test was performed on a full-scale model of a one-sixth sector fuel element interface and simulated bypass channel. The scatter in the experimental data was appreciable, amounting to about one decade in the crossflow loss coefficient parameter (K/A^2). The loss coefficients used in the analysis were derived by taking the log mean of the test results, which corresponds to an uncertainty factor of about 3.2. This is well within the uncertainty range investigated. When the expected uncertainty is applied in the adverse direction, the outlet temperature of the high-powered region increases from about 24° to 46°F above the core average.

An analysis was made to determine the effects of crossflow on the detailed flow in the central control rod column of the maximum powered fuel region, Region 24. The primary inducement for crossflow is the dimensional change in the graphite resulting from thermal gradients and irradiation effects. The dimensional changes cause finite gaps at the fuel element interfaces where crossflow can occur. Calculations based on a control rod coolant flow of 4% of the flow through the core indicate that the maximum temperature difference in the graphite element is about 400°F. This occurs in the bottom two fuel elements in the active core and is for a relatively high-powered region with a radial peaking factor of 1.5. Assuming complete relaxation of thermal stresses in the graphite, the axial dimensional change in a 31-in. element is about 0.05 in. Actual dimensional changes in graphite are a complicated function of temperature, flux, and time. For this reason, two hypothetical interface gap systems were analyzed, one with a maximum gap of 0.05 in., the other with gaps arbitrarily increased by a factor of two (excluding top reflector gaps). The interface gap assumed for each axial joint is shown in Table 3.3.

Figure 3.1 shows the pressure in the selected coolant channels versus distance from the top of the core for case 1 (maximum interface gap equals 0.05 in.). The pressure in the coolant holes shown represents the range of pressure to be expected at each axial plane. The pressure in the bypass channel falls approximately in the middle of the range of pressures for the coolant holes. The pressure in the control rod hole is essentially constant over the core length upstream from the discharge orifice. All of the core pressure drop (excluding losses in the inlet orifice) occur across the control rod outlet orifice. The effect is that pressure in the control rod hole is always larger than that in the coolant holes or in the bypass channel. The direction of crossflow is therefore always out of the control rod

hole. The maximum pressure difference between the control rod hole and the coolant holes is about 4 psi and occurs below the active core.

Table 3.3
INTERFACE GAPS ASSUMED FOR ANALYSIS OF CROSSFLOW
IN THE CONTROL ROD COLUMN

Element No. from Top of Core	Interface Number	Interface Gap (in.)	
		Case 1	Case 2
Top reflector:			
1			
2	1	0.002	0.002
3	2	0.002	0.002
Active core:			
4	3	0.002	0.005
5	4	0.015	0.03
6	5	0.025	0.05
7	6	0.035	0.07
8	7	0.045	0.09
9	8	0.05	0.10
Bottom reflector:			
10	9	0.038	0.075
11	10	0.025	0.05
12	11	0.025	0.05

Additional insight into the effects of crossflow can be obtained by comparing the flow rate per coolant hole with a reference coolant hole with no crossflow. If crossflow could be reduced to zero, the total flow to the coolant holes would be 3.18 lb/sec, or an average of 0.0576 lb/sec per coolant hole. The corresponding flow in the control rod holes would be 1.0 lb/sec (the total for the two holes, based on 4% of the total flow through the core). The results with crossflow are presented in Table 3.4. It can be seen that the effect of crossflow is significant. In the top fuel element, the total flow in the control rod holes is about twice the design flow, 2.17 versus 1.0 lb/sec for case 1, while the flow in the coolant holes is about 80% of the reference flow for no crossflow. Inflow into the coolant holes improves the situation toward the bottom of the core. In the bottom fuel element, the flow in the control rod holes is 30% greater than design, and the flow in the coolant holes is about 10% greater than the reference flow. The results for case 2, in which the size of interface gaps is twice that of case 1, indicate that the effects of crossflow are slightly more pronounced than for case 1.

Full-scale Orifice Vibration and Flow Test. Comprehensive analyses of flow and pressure data were performed this period in an effort to establish in detail the pressure loss characteristics of the orifice and to confirm

Fig. 3.1--Pressure distribution in the control rod column of fuel rod region 24

Table 3.4

ALLOCATION OF COOLANT FLOW IN THE CENTRAL CONTROL ROD COLUMN
(Region 24, Inlet Orifice Fully Open, Flow Rate given in lb/sec)

	Case 1		Case 2	
	Maximum Interface Gap = 0.05 in.		Maximum Interface Gap = 0.1 in.	
	Top Fuel Element	Bottom Fuel Element	Top Fuel Element	Bottom Fuel Element
Control rod channels (total flow to two holes)	1.56	1.10	1.86	1.24
Reserve shutdown channel	0.61	0.22	0.78	0.30
Sum of 1 and 2	2.17	1.32	2.64	1.54
Coolant holes (average flow/hole)	0.0473	0.0620	0.0439	0.0628
Reference coolant hole with no crossflow	0.0576	0.0576	0.0576	0.0576
Total flow through column	5.80	5.09	6.08	5.35

the observed higher-than-anticipated fully open loss coefficient K_{min} .^{*} Discrepancies were encountered that resulted in an apparent exponential increase in K_{min} with increasing throat Reynolds number over the full range tested. Investigations to date indicate that the observed Reynolds number dependency is due to a combination of compressibility effects during the air flow tests and uncertainties at high flow rates in the venturi calibration used for determination of mass flow rate through the test rig. The data reduction code has been completely rewritten to include compressibility relations, and recalibration of the venturi has been initiated. Additional analyses are also being made of data from the calibration of the 0.45 scale multicolumn test rig orifices. It appears that upon resolution of compressibility effects and possible flow errors, there will be good correlation between the 0.45-scale and full-scale models.

Fission-product Transport Calculations were completed and fission-product inventories and activity data were compiled for safeguards review documents covering reactor powers of 842-Mw(t) and 879-Mw(t) operation. These data were calculated for PyC-coated fuel particles with an additional SiC-barrier coating layer.

Calculations have been made to estimate an upper limit of hydrolysis that could occur to core fuel particles that have experienced prior coating failure. A simplified and conservative model was set up based on the steam inleakage, core temperatures, coolant flow, and system pressure data for 879-Mw(t) reactor operation.

Three cases of system maloperation were evaluated: failure of the moisture monitors, wrong-loop dump, and failure to dump. The calculational model used considered mass transport of H_2O across the coolant boundary, transport through graphite, and reaction with fuel particle dicarbide. No allowance was taken into account for the depletion of the H_2O vapor due to reaction with the element graphite, fuel rod matrix, or particle coating.

The results of these calculations showed that about 0.12% of the core fuel particle dicarbide may be hydrolyzed for any steam inleakage situation involving wrong-loop dump, moisture monitor failure with two-loop dump, or failure to dump. An estimated time for primary circuit depressurization and steam removal was considered in the cases of wrong-loop dump and failure to dump.

In the case of steam inleakage due to a subheader rupture followed by normal protective action, about $4 \times 10^{-4}\%$ of the total core fuel particle dicarbide may be hydrolyzed for a single occurrence.

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^{*} K_{min} is the fully open, or minimum, orifice loss coefficient. K is the number of velocity heads of pressure loss based on the top reflector coolant channel velocity which are taken across the valve.

MATERIALS

The objectives of this subtask are to:

1. Determine the characteristics of beds of bonded coated particles by means of fabrication technique investigations, thermop property tests, vibration and impact tests, and measurements of packing density and loading uniformity.
2. Select the type of particle bed to be used in the reactor based on the above work and work carried out in other parts of the program, taking into account the expected characteristics and relative ease of reprocessing, and determine the suitability of the selected system by means of further tests of the type described above.
3. Determine the characteristics of various candidate graphites for use in the reactor fuel elements by means of measurements of the physical, thermal, and mechanical properties of these graphites.
4. Determine the characteristics of various control materials by means of measurements of physical, mechanical, chemical, and thermal properties.
5. Determine the irradiation stability of candidate graphites for use in the fuel elements by means of irradiation tests conducted in the Engineering Test Reactor at the temperatures and to the integrated fast flux expected in the plant, followed by postirradiation examination. Approximately five capsules are planned for this part of the program.
6. Establish the effect of fast flux on fuel particle coating structural integrity as a function of coating structure and exposure, by means of irradiation tests in the Engineering Test Reactor and other reactors on unfueled samples of particle coatings, postirradiation measurements of coating properties, such as dimensional changes, flexural strength and crystallite spacing, and subsequent reirradiation to design integrated fast flux and remeasurement. These irradiations will be carried out in the graphite capsules used in 5 above, and in the coated particle capsules used in 7 below.
7. Determine the irradiation stability of coated fuel particles for use in the reactor by means of a series of (1) short-term tests on coated fuel particles to select a coated fuel particle(s) having over its design life a high degree of structural integrity suitable for use in a nonvented fuel element, followed by (2) longer-term irradiation tests conducted at design temperature, to design burnup and to a significant fraction of the integrated fast flux expected in the plant. The "significant" integrated fast dosage will be determined from the results obtained in 6. Capsule tests will be followed by postirradiation examination of the particles. Approximately seven short-term and three longer-term capsules are planned for this part of the program.

Particle Fuel Systems

The injection-molding and wet-molding processes for fabricating fuel rods initially developed under this program were used in making fuel rods for the first proof test element. Some modifications in the process were necessary to overcome the problem of occasional cracking of the weaker particle coatings caused by strong matrix bonding with subsequent shrinkage. In general, this was accomplished by controlling the strength of the matrix and matrix-to-particle bond. The general investigation of the problem and its solution and application in making rods for the first proof test element is covered in the previous quarterly report (GA-8420). Although this successful application demonstrated the feasibility of the process, certain features such as rod strength and dimensional stability needed to be improved. The fuel rod development work has therefore continued, with the main objective being to establish an improved process for fabricating fuel rods with the following desirable properties:

1. Little or no stress on the bonded coated particles.
2. Increased dimensional stability after carbonization.
3. Increased strength after carbonization.
4. Capability of being carbonized in place in the graphite fuel element.

The development effort to achieve these objectives was directed towards three main areas including expanding filler materials, dry mix materials, and porous binder formulations.

Expanding Filler Materials. It has been reported that by proper treatment, a bisulfate residue compound can be formed from highly ordered natural flake graphite that will expand when heated in excess of 300°C (Refs. 1,2). By including the right amount of this material in the binder formulation, it may be possible to control the contraction or expansion of the matrix during carbonization. Samples of residue compound were made by treating some grades 6353 and 850 natural flake graphites in a solution of 12M sulfuric acid and 0.1M nitric acid for 24 hr at room temperature. The residue was first washed with 8M sulfuric acid, then repeatedly washed with distilled water and centrifuged. The material was dried under vacuum at 120°C. A number of bonded matrix samples were prepared using both of the treated filler materials with phenolic and furfuryl resins as binders. These samples were cylinders 1/4 in. in diameter by 1/4 in. long molded in a graphite holder using the regular cure and carbonizing cycles. All the samples showed net shrinkage. Samples of the straight filler material showed no change in volume when heated through the carbonizing cycle to 900°C. Prior to carbonization, analysis of the treated grade 6353 showed 0.20% sulfur and 0.024 milliequivalents of acid per gram of material, while the grade 850 showed 0.26% sulfur and 0.116 milliequivalents of acid per gram of material. This analysis along with the fact that the material showed no change on heating would indicate that very little residue compound was formed. Although more drastic reaction conditions may produce the desired product, work in this area was stopped in favor of more promising approaches.

Dry Mix Materials. The large amount of shrinkage that occurs when the present matrix is carbonized is due in part to the high content of liquid

resin. If a much larger amount of solid filler material could be used, less shrinkage would occur on carbonization. With the present formulations, even a small amount of extra filler material increases the viscosity to the point that injection molding cannot be used.

One means of incorporating more solid material is to blend a proportionately larger amount of filler material with a dry powdered resin that will melt and sinter on heating. This dry mix could be added by vibration along with the particles at the blending-loading step followed by a regular cure. Several small 1/4 by 1/4 in. cylinder specimens were made from various phenolic resin - graphite powder dry-mix formulations such as 50 wt-% grade 7031A - 50 wt-% GP-38, 25 wt-% grade 7031A - 75 wt-% GP-38, 75 wt-% grade 7031A - 25 wt-% GP-38, 50 wt-% grade 8839 - 50 wt-% GP-38, and 50 wt-% grade 11078 - 50 wt-% GP-38. These specimens sintered to a hard mass and retained fairly good strength after carbonization, except for the specimen containing 25 wt-% resin. A fuel rod specimen was then made by hand-blending fuel particles in a 50 wt-% grade 7031A - 50 wt-% GP-38 dry mix and loading in a mold. The rod had good strength after sintering and curing, and was carbonized unrestrained. The rod held together during carbonization but considerable warpage and cracking occurred. Several attempts were made to set up a vibratory feeder to feed the dry blend mix into the loading funnel along with the fissile and fertile particles and achieve a uniform loading of all components in the mold. This approach, however, was only partially successful, since it was difficult to obtain a uniform distribution of the dry mix throughout the rod.

Porous Binder Matrix Formulations. This approach towards making fuel rods with the desired properties appears to be very promising. It is based on new binder formulations that use a lower viscosity phenolic resin and much higher amounts of curing agent to obtain a porous, low-density type of carbon matrix. The larger amounts of curing agent promote an accelerated or exothermic curing of the resin that leaves many gas bubbles and pores in the hardened matrix material. This porous or foamy structure is maintained through the carbonization. During carbonization, the fuel rods show an initial expansion of 1 to 3 mils and then stabilize. This net expansion (opposed to the contraction obtained with previous formulations) along with the open porous structure and the fact that these formulations have better release characteristics from the coated particle surfaces results in much less stress being applied to the particles by the matrix. This greatly reduces or eliminates the possibility of the particle coatings being cracked during the fabrication process, and gives a rod with good dimensional stability and a fair amount of strength. A metallographic section of a typical porous matrix fuel rod shown in Fig. 3.2 can be compared with the former dense matrix shown in Fig. 3.3. A number of different porous matrix formulations have been investigated. Most of these have used grade R72S, a low-viscosity resol-type phenolic resin, along with maleic anhydride as the curing agent. Two different methods of incorporating the solid maleic anhydride in the R72S were used. One was to dissolve the maleic anhydride in furfuryl alcohol to obtain a 1:1 solution, which was added to the resin. This solution was very reactive, however, because the maleic anhydride rapidly polymerizes the furfuryl alcohol and increases the viscosity, even under refrigeration. The other and more satisfactory approach was to use a mixed anhydride curing agent. The maleic anhydride was dissolved 1:1 in Nadic methyl anhydride,

Fig. 3.2--SiC-PyC-coated fissile and fertile particles in injection-molded fuel rod with 43.8% R72S phenolic resin, 29.2% mixed anhydride curing agent, 18% graphite powder, 9% charcoal powder filler matrix; note porous and void areas in matrix

Fig. 3 3--SiC-PyC-coated fissile and fertile particles overcoated with Spraygraph mold release in an injection-molded fuel rod with a 70% phenolic resin, 20% graphite powder, 10% charcoal powder filler matrix; note dense matrix

the liquid methylated maleic acid adduct of phthalic anhydride. This stable solution can then be easily blended with the R72S. A series of fuel rod sections containing matrix formulated with 20 parts, 30 parts, and 40 parts of mixed anhydride per 100 parts resin can be seen in Figs. 3.4, 3.5, and 3.6, respectively. These illustrate how the porosity of the matrix increases slightly with increasing amounts of mixed anhydride.

The filler material also has an important effect on the structure of the matrix and expansion of the rod during carbonization. The basic filler mix used in a number of the formulations consists of two parts GP-38 graphite powder and one part B-C 589 charcoal powder, both sized to less than 30 μ particle size. Sections of fuel rods containing 27, 24, and 18 wt-% filler in the phenolic resin - mixed anhydride binder mix (100:40) are shown in Figs. 3.7, 3.8, and 3.9, respectively. Again, note the increasing porosity with decreasing amount of filler. These rods had average diametral expansions during carbonization of 0.6 mils, 2.5 mils, and 3.0 mils, respectively.

Some of the earlier rods in this investigation contained particles that had been surface-treated with a wax release agent. The particles used in later formulations (based on R72S resin) had no surface treatment.

Curing cycles of 16 hr at 90°C followed by 4 hr at 120°C were used for the earlier rods along with a carbonization cycle consisting of 39-hr heatup to 900°C. The use of the increased amounts of curing agent in the formulations allowed the curing time to be reduced to 1 hr at 100° to 105°C. The increased porosity obtained in the matrix allowed the carbonizing cycle to be reduced to a 16-hr heatup to 900°C. In addition, carbonization is now being done in slightly oversize graphite tube holders (0.471 in. and 0.475 in.) simulating carbonizing in the graphite element. After curing, the rods have a nominal diameter of 0.466 in. During carbonization they undergo a 1- to 3-mil diametral expansion, depending on the formulation. However, this expansion appears to be independent of whether they are carbonized in a 0.471- or a 0.475-in. hole. This stabilization, therefore, definitely points up the feasibility of carbonizing the rods in the graphite elements with adjustments for the desired diameter and radial gap. This, along with the other rod characteristics resulting from the new formulations, meets to a large extent the goals set up for the improved rod development effort. Work on further refinements of the formulations will continue.

Coated Particle Crushing Strength. Among the factors that must be considered in the design of the TRISO-coated fuel particles is the ability of the coated particles to withstand compressive loads imposed on them during the manufacturing of the fuel rods. Studies have therefore been conducted to determine the relationship between coated-particle design and crushing strength.

Crushing loads were determined for TRISO-coated particles as a function of various coating thickness parameters. High-resolution radiomicrographs were used to determine the dimensions of the particles and of the various coating layers. The coating thickness parameters evaluated were separated into two groups:

Fig. 3.4--SiC-PyC-coated fissile and fertile particles in injection-molded fuel rod with 60.8% R72S phenolic resin, 15.2% mixed anhydride curing agent, 16% graphite powder, 8% charcoal powder filler matrix

Fig. 3.5--SiC-PyC-coated fissile and fertile particles in injection-molded fuel rod with 53.2% R72S phenolic resin, 22.8% mixed anhydride curing agent, 16% graphite powder, 8% charcoal powder filler matrix

Fig. 3.6--SiC-PyC-coated fissile and fertile particles in injection-molded fuel rod with 45.6% R72S phenolic resin, 30.2% mixed anhydride curing agent, 16% graphite powder, 8% charcoal powder filler matrix

Fig. 3.7--SiC-PyC-coated fissile and fertile particles in injection-molded fuel rod with 43.8% R72S phenolic resin, 29.2% mixed anhydride curing agent, 18% graphite powder, 9% charcoal powder filler matrix

Fig. 3.8--SiC-PyC-coated fissile and fertile particles in injection-molded fuel rod with 45.6% R72S phenolic resin, 30.4% mixed anhydride curing agent, 16% graphite powder, 8% charcoal powder filler matrix

Fig. 3.9--SiC-PyC-coated fissile and fertile particles in injection-molded fuel rod with 49.2% R72S phenolic resin, 32.8% mixed anhydride curing agent, 12% graphite 6% charcoal powder filler matrix; note increased porosity

Group I:

Variable PyC thickness; with buffer and SiC thickness held constant to within $\pm 10 \mu$ and $\pm 5 \mu$, respectively.

Group II:

Variable SiC thickness; with buffer and PyC thickness held constant to within $\pm 10 \mu$ and $\pm 5 \mu$, respectively.

The particles were crushed using a small testing machine equipped with a sensitive low-level load cell. This procedure allowed testing of individual particles whose coating thicknesses were accurately known. The crushing loads were measured with an over-all accuracy of ± 50 g. The experimental results are shown plotted in Figs. 3.10 through 3.14.

The effect of increasing the isotropic PyC thickness on fissile-size particles having a 50- μ buffer coating and a 23- μ SiC coating is shown in Fig. 3.10. Little increase in strength occurs until about 15 to 20 μ of PyC is added over the SiC coating. Beyond this thickness, a linear increase in crushing load occurs with increasing PyC thickness. The slope of the curve is about 30 g/ μ PyC.

The influence of SiC coating thickness on fissile-size particles is shown in Fig. 3.11. For particles with 50- μ buffer coatings and 60- μ isotropic coatings, the addition of less than about 15 μ of SiC results in a decrease in crushing load. Thicker SiC coatings increase the crushing strength of the TRISO-coated particles, though the increase in strength is not quite linear with SiC thickness. A factor of two improvement in crushing strength is obtained with a 20- to 25- μ SiC coating added to the BISO. This is further illustrated in Fig. 3.12.

Similar results for the larger, fertile-size particles are shown in Figs. 3.13 and 3.14. Figure 3.13 is similar to Fig. 3.11, but shows that the SiC layer must be at least 30- μ thick to achieve the crushing loads of the equivalent BISO coating on these large particles; below this minimum SiC thickness, BISO coatings are stronger. The effect on crushing load with increasing thickness of the PyC layer on a 29- μ SiC coating is shown in Fig. 3.14. Increasing the PyC thickness beyond about 35 μ results in no increase in coating crushing load because of the relative thinness of the SiC layer. A thicker SiC coating should yield a curve similar to that of Fig. 3.10.

The data indicate that thin SiC layers added to the BISO coating can reduce its crushing strength. This is tentatively explained as follows. For a heavy-walled sphere under bearing load, the highest tensile stresses are located at the inside diameter along the load axis. In a TRISO particle, this stress would occur in the SiC, which has an elastic modulus considerably greater than that of PyC. Thus, it would be highly probable that failure would occur in the SiC first, followed by a failure of the PyC coating due to high localized stresses at the crack tips in the SiC layer. Thicker SiC coatings would require higher loads to achieve failure. In this latter case, failure of both coatings would probably occur simultaneously, since the PyC would be unable to absorb the strain energy released by the fractured SiC layer.

Fig. 3.10--Crushing force versus outer PyC coating thickness for fissile-size TRISO particles

Fig. 3.11--Crushing force versus SiC coating thickness for fissile-size TRISO particles

Fig. 3.12--Crushing force versus PyC coating thickness for fissile-size TRISO and BISO particles

Fig. 3.13--Crushing force versus SiC coating thickness for fertile-size TRISO particles

Fig. 3.14--Crushing force versus outer PyC coating thickness for fertile-size TRISO particles

The following general conclusions can be made based on these mechanical strength data:

1. For fissile-size particles, a minimum SiC thickness of 15 μ is required to achieve BISO coating strengths. For fertile-size particles this minimum appears to be about 30 μ of SiC.
2. For SiC thicknesses of 20 to 25 μ , fissile-size TRISO particles are twice as strong as equivalent BISO particles. However, fertile-size TRISO particles with the same SiC coatings (20 to 25 μ), while not as strong as the corresponding BISO coating, are as strong as the TRISO-coated fissile particles.
3. The crushing loads for fissile-size TRISO-coated particles containing a 23- μ layer of SiC can be increased by increasing the PyC thickness. On fertile-size particles, the SiC layer must be thicker than about 30 to 35 μ to get a similar benefit from increasing the PyC thickness.

GRAPHITE DEVELOPMENT

The objective of this program is the evaluation of various conventional needle-coke and a few nominally isotropic graphites for use in the PSC reactor core. The graphites presently under examination have been obtained from various vendors and are being evaluated with respect to purity, thermal properties, mechanical properties, and cost for use as a moderator and structural lattice for fuel elements and as the surrounding neutron-reflecting material. The graphites under consideration for the above applications are grades H327, H328, 9567, RC4 (formerly designated 9345 or SX4), and TS-688.

A cumulative summary of the results from tests for physical and mechanical properties of unirradiated PSC candidate core graphites have been compiled in Table 3.5. The physical properties of these candidates have been placed near the bottom of this table: k , thermal conductivity; CTE, mean value of thermal expansion coefficient; and ρ_B , bulk density. In the upper portion of the table the mechanical properties have been grouped: UTS, ultimate tensile stress; UCS, ultimate compressive stress; MOR, modulus of rupture (in flexure); ϵ_F , total strain to fracture; μ , Poisson's ratio; E_T , E_F , and E_D , the modulus of elasticity as determined by tensile, flexural, and sonic resonance tests, respectively. Preliminary measurements of E_D between 22° and about 1000°C revealed a slight increase (about 5%) in E_D (perpendicular) and a small decrease (about 7%) in E_D (parallel) for grade H327 graphite.

A study is being conducted on the diffusivity of Cs in candidate fuel element graphites. As part of this work, measurements of helium permeability, pore-size distribution, bulk density, and apparent density have been made for comparison with the diffusivity measurements to establish the relative merits of these candidates in regard to fission-product diffusivity. The porosimetry results are presented in Table 3.6. The designation 9567(N3P) describes the Gulf General Atomic purified Nuclear III purity material, which was the only 18-in.-diameter stock of grade 9567 available at the time. Samples of 18-in.-diameter 9567(N2), or Nuclear II purity material, will be evaluated later. The closed-pore volume fraction remains essentially the

Table 3.5
PHYSICAL AND MECHANICAL PROPERTIES OF UNIRRADIATED PSC CANDIDATE CORE GRAPHITES^a

Parameter	H327, 13-in.-Diam		H327, 18-in.-Diam		AGOT-NC (TS-688), 18-in.-sq		AGOT-NP (TS-814), Impregnated 17-in.-Diam		9567 ^c , Nuc. III, 18-in.-Diam	
		⊥		⊥		⊥		⊥		⊥
UTS, 10 ³ psi	2.85 (±0.30)	1.14 (±0.14)	1.68 (±0.76)	1.04 (±0.08)	1.57 ---	0.93 ---	1.71 ^b ---	1.93 ^b ---	1.49 (±0.27)	1.26 (±0.16)
E _T , 10 ⁶ psi	---	-0.77 ---	1.75 (±0.41)	0.60 (±0.09)	-1.73 ---	-0.95 ---	-2.07 ^b ---	-0.86 ^b ---	1.48 (±0.19)	0.62 (±0.07)
ε _F , 10 ⁻³ in./in.	---	1.7 ---	1.3 (±0.5)	2.6 (±0.3)	1.4 ---	1.7 ---	---	---	1.3 (±0.2)	3.0 (±0.7)
UCS, 10 ³ psi	---	---	3.68 (±0.89)	3.53 (±0.45)	---	---	---	---	---	---
MOR, 10 ³ psi	4.17 ---	2.20 (±0.23)	3.06 ^b ---	1.58 ^b ---	2.05 ---	---	2.37 ^b ---	1.77 ^b ---	---	---
E _F , 10 ⁶ psi	1.67 ---	0.65 (±0.07)	---	---	1.61 ---	---	---	---	---	---
E _D , 10 ⁶ psi	1.46 (±0.04)	0.83 (±0.02)	-2.30 ^b ---	-0.95 ^b ---	---	---	---	---	---	---
μ	0.25 (±0.06)	0.21 (±0.06)	---	---	---	---	---	---	---	---
k, cal/cm-sec-°C:			(22° - 600°C)							
Value at 600°C	---	0.13	---	-0.21 ^b	---	0.13	---	---	0.23	0.14
Value at 1000°C	---	0.10	---	-0.12 ^b	---	0.10	---	---	0.14	0.10
CTE, 22° - 1000°C, 10 ⁻⁶ °C ⁻¹	1.67 (±0.38)	4.06 (±0.33)	1.97 (±0.19)	4.23 (±0.06)	2.02 (±0.33)	3.98 (±0.35)	(22° - 50°C)		1.34 (±0.06)	3.66 (±0.22)
σ _B , g/cm ³	1.78 (±0.01)		1.81 (±0.03)		1.66 (±0.08)		1.79 ^b ---		1.74 (±0.01)	

^aAll measurements at room temperature unless otherwise indicated. Values in parentheses are standard deviations.

^bDenotes data supplied by vendor; no comparable Gulf General Atomic data yet available.

^cNo Gulf General Atomic data yet on 9567 (Nuclear II) 18-in. diameter; should be close to property levels shown for Nuclear III purity material.

Table 3.6
POROSIMETRY DATA LOG-RADIAL SAMPLES

Grade	Log No.	Log Diam (in.)	Sample ^a No.	Open-pore Volume Fraction	Closed-pore Volume Fraction	Bulk Density (g/cm ³)	Apparent Density (g/cm ³)	<u>Open Vol</u> <u>Closed Vol</u>
H327	3	18	1-0	0.110	0.060	1.88	2.16	1.88
H327	3	18	2-0	0.108	0.056	1.89	2.12	
H327	3	18	2-I	0.117	0.060	1.87	2.11	2.23
H327	3	18	3-I	0.119	0.046	1.90	2.15	
9567(N2)	X	6	1-0	0.133	0.067	1.81	2.09	1.90
9567(N2)	X	6	4-0	0.125	0.068	1.83	2.09	
9567(N3P)	6A2-13	18	3-I	0.176	0.042	1.77	2.15	3.74
9567(N3P)	6A2-13	18	2-I1	0.154	0.057	1.79	2.12	
9567(N3P)	6A2-13	18	2-I2	0.185	0.044	1.75	2.15	
9567(N3P)	6A2-13	18	2-0	0.200	0.040	1.72	2.15	4.80
9567(N3P)	6A2-13	18	3-0	0.193	0.043	1.73	2.15	
9567(N3P)	6A2-13	18	4-0	0.197	0.040	1.73	2.16	
RC4	X	14	1-I	0.137	0.099	1.73	2.01	1.38

^a The 0 on the sample number indicates an outer-radial or near-edge location; the I indicates an inner-radial or near-center location.

same for inner (near center) samples and outer (near edge) samples in grades H327 and 9567(N3P). Grade 9567(N2) manifests about 50% greater closed-pore volume fraction than does 9567(N3P), for the outer samples. Grades 9567(N2) and H327 seem to have about the same amount of closed porosity. Grade RC4 exhibits almost twice as much closed-pore volume fraction as the needle-coke grades. The open-pore volume fraction increases slightly for grade H327 from the outer to the inner samples, whereas just the opposite in the case for grade 9567(N3P). The open-pore volume fraction for grades 9567(N2) and RC4 are nearly the same, both being intermediate between grades H327 and 9567(N3P). The differences in pore-volume fractions between these grades, and between inner and outer locations in the same grade, probably arise primarily as a result of differences in the processing (especially the baking and the impregnation steps).

The He permeability results are presented in Table 3.7. The permeability constant, K , for grade H327 is approximately $0.1 \text{ cm}^2/\text{sec}$ at the near-edge location. Thus, grade H327 exhibits greater permeability at near-center locations than at near-edge locations by about a factor of three. Such variation in permeability probably arises as a result of differences in effects of the extrusion and/or impregnation steps during manufacturing. There is essentially no radial variation in grade 9567(N3P) which has about the same level of permeability as does the near-edge location in H327 and the near-center location in TS-688. The permeability constant for the near-center location in the "isotropic" grade RC4, surprisingly, is lower than for the needle-coke grades: about 24 times the values for TS-688 and 9567(N3P), and about 50 times the value for H327.

A second group of three full-size (18-in.-diameter) logs representative of H327 graphite recently have been received, and two of these are being sampled for thermal and mechanical properties. In addition, three full-size logs of 9567 (Nuclear II purity) graphite have been received along with two other logs of TS-814 for a similar type of evaluation.

Efforts on the data-storage and selective-retrieval system for PSC graphite data have continued during this period. Drafts of card-punching input sheets for this system have been prepared to permit a shakedown of potential problems.

Measurement of mechanical and thermal properties of the 18-in.-diameter logs of H327 has begun. A critical evaluation of tensile test procedures and specimen configuration has been initiated in conjunction with these measurements.

Control Material

Reserve Shutdown System. The reference reserve shutdown system contains 0 132- to 0 437-in.-diameter B_4C granules that would be released into the core to effect shutdown.

Coating of B_4C granules with pyrolytic carbon (PyC) is being studied to minimize dusting. In addition, a smooth PyC coating should reduce any tendency of B_4C particles to clog in the hoppers. Another potential benefit

Table 3.7
PERMEABILITY DATA^a

Grade	Log No.	Log Diam (in.)	Sample No. ^b	Run	Temp (°F)	Pressure (psig)	ϕ^c (10^{-8} cm ³ /sec ²)	K^d (10^{-2} cm ² /sec)
H327	3	18	1-0	{ 1 2	69.0 69.0	0.50 1.00	3.04 6.14	6.02 12.58
H327	3	18	2-0	1	74.0	0.50	5.27	10.44
H327	3	18	2-I	{ 1 2	74.5 74.0	0.49 0.49	11.88 11.87	23.53 23.51
H327	3	18	3-I	{ 1 2	74.5 69.0	0.49 0.49	21.83 18.79	43.25 37.23
9567 (N2)	X	6	1-0	{ 1 2	70.5 70.5	0.63 0.50	0.25 0.29	0.50 0.57
9567 (N2)	X	6	4-0	1	74.5	0.50	0.09	0.18
9567 (N3P)	6A2-13	18	2-I-1	{ 1 2	71.0 71.5	0.50 0.50	6.36 6.40	12.61 12.69
9567 (N3P)	6A2-13	18	2-I-1	{ 1 2	72.0 73.5	0.51 0.50	8.00 7.48	15.87 14.83
9567 (N3P)	6A2-13	18	3-I	{ 1 2 3 4	73.5 74.0 73.0 72.0	0.50 0.51 0.50 0.50	7.79 6.76 7.64 7.06	15.44 13.44 15.14 13.99
9567 (N3P)	6A2-13	18	4-0	{ 1 2	72.5 74.0	0.52 0.50	6.37 5.47	12.63 10.84
9567 (N3P)	6A2-13	18	2-0	{ 1 2	73.0 74.0	0.51 0.50	5.99 5.87	11.89 11.63
9567 (N3P)	6A2-13	18	3-0	{ 1 2	74.0 74.0	0.51 0.50	8.90 8.65	17.65 17.15
TS688	A	18 (sq.)	LA-CB-I	{ 1 2	75.0 68.0	0.50 0.50	6.25 6.23	12.38 12.35
RC4	X	14	1-I	1	73.0	0.50	0.28	0.55
RC4	X	14	3-IA	{ 1 2	75.0 74.5	0.56 0.57	0.22 0.22	0.44 0.43

^a All data for He flow along perpendicular direction in log.

^b The 0 on the sample number indicates an outer-radial or near-edge location; the I indicates an inner-radial or near-center location.

^c Permeability, ϕ , is defined by Darcy's Law as $Ql\eta/APt$, where Q is the total fluid flow during test time t through a sample with length l in the flow direction and cross-section area A normal to flow, for a fluid with viscosity η flowing through a pressure differential P applied across the sample.

^d Permeability, K , is defined as Ql/At , a special case of ϕ for unchanging P and η from test to test.

from a PyC coating may be increased thermal stability of the B_4C . The possibility of reaction and fusion between granules would be eliminated and impurities such as B_2O_3 would be retained by the coating at high temperatures.

During the quarter a number of coating runs were made to demonstrate the feasibility of coating B_4C and to determine the relative dusting rates of coated and uncoated B_4C under simulated storage conditions and thermal shock. The coatings were deposited on the B_4C granules described in GA-8270. Only granules with dimensions of 1/4 in. or less were used in the coating experiments.

A fluid bed coater (Ref. 3) with a 1.5-in.-ID coating tube was used. Total helium-hydrocarbon flow rates were varied from about 11,000 to 20,000 cm^3/min . It was necessary to include 80 g of PyC-coated UC_2 spheres (approximately 500 μ diameter) with a 50-g batch of B_4C in order to fluidize the bed. Several coating conditions were explored. Normally a buffer was deposited first from a helium-acetylene mixture on the B_4C granules at 1000° to 1200°C to rapidly fill in depressions. The buffer was overcoated with PyC deposited from a helium-propane mixture at 1400°C. The thickest coating produced in this work varied from 50 to 600 μ and was deposited using two successive buffer-overcoat coating cycles. The conditions for this coating are shown in Table 3.8.

Table 3.8
COATING CONDITIONS YIELDING 50- TO 600- μ PyC COATING ON B_4C GRANULES

Coating Layer No.	Temp. (°C)	Acetylene Flow Rate (cm^3/min)	Propane Flow Rate (cm^3/min)	Helium Flow Rate (cm^3/min)	Time (min)
1	1000-1200	6000	--	12,000	4
2	1400	--	5500	13,500	10
3	1000-1200	6000	--	12,000	4
4	1400	--	5500	13,500	10

Figure 3.15 shows typical granules before and after coating under conditions shown in Table 3.8. A photomicrograph of a coated granule cross section is shown in Fig. 3.16. There was no sharp boundary between the PyC layers, but an apparent variation in structure was visible. The variation in coating thickness leading to a more rounded particle is demonstrated in Fig. 3.16. When thinner PyC coatings were deposited, protrusions of the B_4C were not completely covered.

While in storage above the core, the B_4C granules will be exposed to some degree of vibration due to the circulation of the coolant. Vibration tests were therefore conducted on coated and uncoated B_4C granules in order to measure and compare dusting rates. About 91.5 g of either coated or uncoated B_4C was loaded into a polyethylene cylinder. The B_4C column was

Fig. 3.15--B₄C granules prior to coating (above)
and after coating with 50-600 μ PyC (below) (10X)

Fig. 3.16--PyC coating on granular B_4C for coating conditions shown in Table 3.8 (50X)

weighted at the top to simulate the weight of a tall column of B_4C and to ensure that granules near the top of the column remained in contact. The assembly was then vibrated at 60 cps and 7 Gs maximum acceleration. At the end of time intervals of 0.08, 0.33, 5.0, and 50 hr the B_4C was sieved to determine the weight of the material in three size fractions. The dust and granules were reblended at the start of each cycle. The bulk volume of B_4C in the cylinder was measured at the start of each cycle to determine the packing density. The apparent bulk density of the granules was determined by a liquid displacement technique. The results of this work are shown in Table 3.9.

The packing density of the uncoated B_4C after 50 hr of vibration was about the same as that obtained when a pneumatic vibrator was used to produce compaction (see GA-8270). It is interesting to note that the packing density of the coated material was higher than that of the uncoated material, even though the bulk density of the coated B_4C was lower. This may have been the result of closer packing as a result of reduced friction between the coated particles. However, another probable cause was the fact that coating was added primarily in depressions, so that it could add to the weight without significantly increasing the separation between the B_4C particles.

The rate of dust production in uncoated B_4C decreased with increasing time, as would be expected if dust arises primarily from wearing down of contacting asperities. Somewhat more dust was produced in the -74μ than in the $+74$ to -210μ size range. The effect of time on dusting rate in the coated B_4C was difficult to determine because of the extremely slow rate of dust production. After 50 hr of vibration, the amount of dust (-210μ) from uncoated B_4C was about 0.560 wt-% of the original charge as opposed to 0.002 wt-% from the coated material. Thus for a 50-hr test, a factor of about 250 decrease in dust production was produced by the application of a PyC coating.

Coated and uncoated B_4C from the batches described previously were also subjected to thermal shock experiments. A 50-g charge of granules was dropped 34 in. into a closed-end 1-in.-diameter graphite tube that was preheated in helium to $1400^\circ C$. After 1 min in the hot zone, the tube was withdrawn and cooled to room temperature. The material in the tube was then sieved on 14-, 70-, and 200-mesh screens to determine dust production caused by thermal shock. The +14 mesh size fraction was then exposed to a second identical thermal shock.

The results are shown in Table 3.10. A single thermal shock test produced measurable quantities of dust only in the -14 to $+70$ mesh size range for both coated and uncoated B_4C . There was no apparent change in the appearance of the granules except for the absence of coating on the tips of some asperities on thermal shocked coated granules. In repeat tests of the same materials about the same weight of dust was produced. However, the uncoated B_4C tended to produce a smaller particle size. A dust production rate (diameter <1.2 mm) of about 0.04 wt-% per drop was calculated for the uncoated granules and 0.002 wt-% per drop for the coated material. Therefore the dust produced by thermal shock was reduced by a factor of about 20 by the application of a PyC coating.

Table 3.9
DUST PRODUCTION AND PACKING DENSITY OF COATED AND UNCOATED B₄C

Description	Total Weight (g)	Cycle No.	Length of Cycle (hr)	Final Packing Density (g/cm ³)	Percent Change In Packing Density During Cycle	Cumulative Dust Production Size Fraction (wt-%)		
						-74 μ	+74 -210 μ	+210 μ
Uncoated ρ=2.3 g/cm ³	91.5	1	0.08	1.10	5.5	0.063	0.055	Balance
		2	0.33	1.13	5.5	0.312	0.170	Balance
		3	5.0	1.13	2.7	0.550 ^a	0.190	Balance
		4	50.0	1.13	2.7	0.340	0.200	Balance
Coated ρ=2.2 g/cm ³	91.5	1	0.08	1.22	2.5	0.000	0.000	Balance
		2	0.33	1.22	2.5	0.000	0.001	Balance
		3	5.0	1.22	2.5	0.000	0.000	Balance
		4	50.0	1.29	8.4	0.001	0.001	Balance

^a Probably higher than actual because of an inadvertent error.

Table 3.10
RESULTS OF THERMAL SHOCK TESTS

Description	Thermal Shock Test No.	Dust from 50-g Sample (wt-%)		
		Size Fraction		
		-1.2mm, +210 μ	-210 μ , +75	-75 μ
Uncoated	1	0.040	0.000	0.000
Uncoated	2	0.038	0.008	0.006
Coated	1	0.002	0.000	0.000
Coated	2	0.002	0.000	0.000

Irradiation Testing

Irradiation experiments are being conducted to test BISO- and TRISO-coated particles under PSC design irradiation conditions. These experiments are intended to guide the selection of fuel materials that will meet the performance requirements of the PSC reactor. Experiments currently in progress are proof tests intended to demonstrate the irradiation stability of BISO-coated particles under PSC reactor conditions. Postirradiation examination of one irradiation test of TRISO-coated particles has just been completed, and preparations are beginning for two new irradiation experiments to test TRISO-coated particles. Two additional irradiation tests of fuel rods containing TRISO-coated particles are also planned. All experiments are being or will be conducted at the PSC maximum fuel temperature to full design fuel burnup and to a significant fraction of the integrated fast-neutron dose.

Graphite irradiation experiments on the PSC program are directed toward obtaining data on conventional graphites over the temperature range and to the lifetime dose of the reactor. Physical properties and dimensional changes are being measured and the results analyzed to aid in the selection of fuel element graphite.

Capsule P17. Capsule P17 is the first proof test of BISO-coated particles. The coated particle samples were designed in accord with current models of coated particle and pyrolytic carbon irradiation behavior to give a range of merit factor (Refs. 4,5) and strain rate (Refs. 5,6) values. Both BISO-HTI and BISO-LTI coatings are included. The desired merit factors and strain rates were obtained by systematically varying the structure and thickness of the outer isotropic layer while the thickness and density of the inner buffer layer were held constant. Most of the coating types are being tested in triplicate on cores with different Th:U ratios to ensure that at least one sample will be close to the design burnup despite ETR flux variations. All samples are being tested on fissile size cores. Detailed descriptions of the coated particle samples were given in a previous quarterly report (GA-8038)

The fuel temperatures in this capsule are being programmed to simulate the thermal history of a typical maximum temperature region in the PSC core.

The temperatures of these regions in the PSC core will increase from 1100° to 1300°C during the first year (to approximately 7% FIMA), drop abruptly to 1100°C at the start of the second year due to the partial refueling, and then gradually decrease from 1100° to 1000°C over the remaining five years (to 20% FIMA). The capsule will be irradiated at 1300°C to approximately 13% FIMA, at 1100°C to 20% FIMA, and at 1000°C for those samples which exceed 20% FIMA. The experiment is now scheduled to be irradiated to a fast-neutron dose of approximately 5×10^{21} n/cm² ($E > 0.18$ Mev), which is over 60% of the expected PSC maximum dose of 8×10^{21} n/cm². The fuel burnups will center around the full PSC design burnup of 20% FIMA.

The P17 capsule is beginning its fifth cycle of irradiation in the I13NE core position of the ETR. The capsule has continued to operate satisfactorily with near-design fuel temperatures of 1080° to 1130°C. (The design temperatures of all cells have now been lowered to 1100°C in accordance with programming the fuel temperatures to simulate the PSC core thermal history.) However, only 7 of the original 15 thermocouples were still operative at the end of the last cycle, and at least three additional thermocouples were lost during the current shutdown due to ETR maintenance operations, which necessitated temporary removal of the capsules and filler piece in the I13 position from the core. The fuel has reached estimated burnups of 24%, 19%, and 16% FIMA (for the three different Th:U ratios on fissile particles) with an estimated fast-neutron dose of 4.4×10^{21} n/cm². The experiment is expected to achieve the design burnups and exposures at the end of this cycle.

Capsule P18. Capsule P18 is the second proof test of BISO-coated particles. It is a longer-term test (six to seven ETR cycles) concurrent with capsule P17 of similar samples designed to achieve a higher fast-neutron dose. The dose will be about 6.5×10^{21} n/cm² ($E > 0.18$ Mev), which is about four-fifths of the expected PSC maximum dose. Some of the samples have fuel cores with different Th:U ratios from capsule P17, so that the burnups will still center around 20% FIMA (PSC maximum) despite the longer exposure. The fuel temperatures are also programmed (as in P17) to simulate the temperature history of a typical PSC maximum temperature core region. Four of the five cells are now designed to operate at a nominal 1300°C (PSC maximum temperature); however, one cell was lowered to 1100°C during the past cycle, and the other cells will be lowered at the start of the next cycle when their fuel burnups reach an appropriate level. A detailed description of the coated particle samples in P18 was given in a previous quarterly report (GA-8038).

The P18 capsule is beginning its fifth cycle of irradiation in the ETR in the I13SW core position. The capsule has been operating well with near-design fuel temperatures on most samples and all 15 thermocouples still operative at the end of the last cycle. However, in the latter half of the past cycle, the fuel temperatures in cells two through five gradually decreased to 50° to 150°C below the design 1300°C, presumably due to a shift in the ETR flux spectrum. Temperatures in these cells are now 1150° to 1250°C. The temperatures in cell 1 (1135° to 1155°C) are close to the design 1100°C. Also, five thermocouples were lost during the current shutdown as a result of the removal and reinsertion of the capsule in the core for ETR maintenance operations (described in the capsule P17 section). The estimated fuel burnups to date are 19%, 16%, and 14% FIMA, with an estimated fast-neutron dose of 4.4×10^{21} n/cm².

Capsule P19. Capsule P19 will be the first P-capsule proof test of BISO-coated particles prepared in large-scale (production) equipment. Several different isotropic layer density and anisotropy factor combinations will be tested for both HTI and LTI coatings to determine the most economical combination that will survive HTGR design irradiation conditions. For HTI coatings, a series is included having isotropic densities of 2.05, 2.01, 1.97, 1.91, 1.83, and 1.74 g/cm³ and anisotropy factors kept as low as possible (1.05 to 1.10). The merit factors of these samples range from 2000 to 2800. Two other HTI samples are included with densities of 1.96 and 1.90, which closely match two samples in the above series; however, these two samples have lower merit factors (1800 and 1900 versus 2750 and 2350), slightly higher anisotropy factors (1.10 and 1.10 versus 1.06 and 1.05), and coatings with thin buffers and circumferential laminations in the isotropic layer. These samples should aid in determining whether it is possible to relax current coating specifications without detrimental effect.

Three LTI samples are included with isotropic densities of 2.01, 1.88, and 1.66 g/cm³ and low anisotropy factors (1.00 to 1.04). Their merit factors range from 1900 to 2500.

One LTI sample was separated into fractions with spherical cores and non-spherical cores, and both fractions will be tested in the capsule to determine the effect of the irregular geometry on irradiation stability.

Capsule P19 is designed to achieve full PSC fuel burnup of 20% FIMA and a fast-neutron dose of 6×10^{21} nvt at the maximum PSC fuel temperature of 1300°C. The fuel temperatures will be programmed to simulate the thermal history of a typical HTGR maximum temperature core region. Three of the samples (two LTI and one HTI) are also duplicated at 900°C to evaluate their stability near the HTGR average fuel temperature. All P19 samples are on fissile particles; samples of BISO-coated fertile particles that were prepared for the first PSC proof test element are included in piggyback positions in P19. A detailed description of the P19 samples is given in Table 3.11.

Assembly of capsule P19 is completed and the capsule is at the ETR site awaiting insertion in April in cycle 95. P19 will be irradiated for six to seven cycles in the 113NW core position. A description of the capsule design is given in Ref. 7.

Capsule P13J. Capsule P13J was a test of TRISO-coated particles designed to determine the optimum coating geometry for this type of particle and to investigate the possibility of a temperature dependence on TRISO coating stability. All samples in this capsule were of the TRISO-I design (buffer-SiC-isotropic), and all had low-temperature isotropic (LTI) pyrocarbon layers with the SiC. (All of the samples were on fissile-size cores.) This was the highest exposure test of LTI coatings. Capsule P13J was irradiated for four ETR cycles to an estimated fuel burnup of 19% FIMA and an estimated fast-neutron dose of 4.0×10^{21} n/cm². In order to evaluate possible thermal effects on TRISO stability, the fuel was irradiated at three design temperatures, 1300°, 1000°, and 600°C, which are close to the maximum, average, and minimum temperatures expected in the PSC core. The fuel temperatures were 1180° to 1310°C in the three 1300°C cells, 950° to 1000°C in the 1000°C cells, and 600° to 630°C in the 600°C cells. Burnup and flux wire analyses are in progress.

Table 3.11
DESCRIPTION OF COATED PARTICLES BEING IRRADIATED IN CAPSULE P19^a
(6-9 Cycle ETR Irradiation)

Lot No.	Type ^b	Coating						Particle Size (μ)	Irradiation Temp (°C) ^c		Analytical Parameters		Remarks
		Buffer		Isotropic			Total Thickness (μ)		Avg	EOL	Merit Factor ^d	Shrinkage Rate ^e (%/10 ²¹ nvt)	
		Thickness (μ)	Density (g/cm ³)	Thickness (μ)	Density (g/cm ³)	Anisotropy Factor							
4000-693E	BISO-HTI	50	0.91	76	2.05	1.09	126	150-250	1300	1100	2600	0.9	HTI density-anisotropy series
4000-649E	BISO-HTI	58	0.82	46	2.01	1.10	104	150-250	1300	1100	1960	1.3	
4000-691E	BISO-HTI	59	0.87	58	1.97	1.06	117	150-250	1300	1100	2750	1.2	
4000-695E	BISO-HTI	47	1.18	68	1.96	1.10	115	150-250	1300	1100	1790	1.6	
4000-658E	BISO-HTI	64	0.69	42	1.91	1.05	106	150-250	1300	1100	2350	1.3	
4000-653E	BISO-HTI	41	0.85	73	1.90	1.10	114	150-250	1300	1100	1880	1.8	
4000-694E	BISO-HTI	53	0.90	76	1.83	1.07	129	150-250	1300	1100	2790	1.9	
4000-652E	BISO-HTI	58	0.84	47	1.74	1.05	105	150-250	1300	1100	1980	2.6	
4000-632E	BISO-LTI	55	0.93	57	2.01	1.04	112	150-250	1300	1100	2070	-1	LTI density series
4000-637E	BISO-LTI	61	0.80	45	1.88	1.04	106	150-250	1300	1100	1880	-2	
4000-660E	BISO-LTI	60	0.88	58	1.66	1.00	118	150-250	1300	1100	2330	-3	
4000-637E	BISO-LTI	61	0.80	45	1.88	1.04	106	150-250	1300	1100	1880	-2	Nonspherical cores
4000-695E	BISO-HTI	47	1.18	68	1.96	1.10	115	150-250	900	900	2390	0.4	Lower temperature HTI and LTI density-anisotropy series
4000-632E	BISO-LTI	55	0.93	57	2.01	1.04	112	150-250	900	900	2760	-0.3	
4000-637E	BISO-LTI	61	0.80	45	1.88	1.04	106	150-250	900	900	2510	-0.5	

^a All particles have a 150- to 250-μ (Th,U)C₂ kernel with a Th:U ratio of 2.5:1 and a density greater than 90% of theoretical. The design burnup is 20% FIMA, and the design fast-neutron dose is 6×10^{21} nvt.

^b HTI denotes high-temperature isotropic pyrocarbon deposited from methane; LTI denotes low-temperature isotropic pyrocarbon deposited from propane.

^c Design values.

^d Calculated by method of Ref. 4 using design conditions (20% FIMA, 1300°C).

^e Shrinkage rate of isotropic PyC coating in parallel direction at total strain of -2%, estimated from data of Ref. 6.

Results from the postirradiation microscopic and metallographic examinations are given in Table 3.12. These results indicate that eleven of the thirteen TRISO samples and both of the BISO samples survived; no sample had more than 6% failure. Three of the SiC samples were designed to evaluate the effect of SiC layer thickness on stability at all three irradiation temperatures. These had SiC layers of 15, 25, and 37 μ , and all were overlaid with thin (8- to 10- μ) LTI layers. The sample with the thinnest SiC layer was purposely designed to fail using the merit factor model (Refs. 4,5). There was a small amount of failure at all three temperatures, but the greatest amount of failure was only 6%. Photomicrographs of this sample are shown in Figs. 3.17 and 3.18. The sample with the thickest SiC layer, which was designed to survive, and the sample with the intermediate thickness SiC layer, which was intended to have a 50-50 chance of survival, both survived at all three temperatures, but the intermediate thickness sample did have very small failure (0.7%) at 1000°C. Photomicrographs of these samples are shown in Figs. 3.19 through 3.22. These results suggest that 1000°C may be a slightly more severe irradiation temperature, although the very small differences in failure rate between 1000°C and the other temperatures are relatively insignificant. Any slight increase in failure at temperatures near 1000°C may be due to the LTI pyrocarbon overcoat, which has a low radiation-induced creep rate in this temperature range. The SiC layer would not seem to be responsible, since irradiation data on unrestrained SiC strips have shown that the saturated linear expansion of SiC increases with decreasing irradiation temperature, i.e., the maximum expansion in SiC should occur at 600°C rather than at 1000° or 1300°C. Also, SiC coatings with high-temperature isotropic (HTI) pyrocarbon layers survived at 1000°C in the P13H experiment, and HTI has a higher creep rate in this temperature range.

The other six samples (tested at 1300°C) all survived. Two, shown in Figs. 3.23 and 3.24, had a thin (10- μ) inner SiC layer with thick outer LTI layers (31 and 52 μ). (These samples are closest to the PSC TRISO reference design.) The second group of two, shown in Figs. 3.25 and 3.26, had thicker, lower-density buffers with SiC layers of 29 and 35 μ and thin (10- μ) LTI overcoats. The final two had BISO coatings, one LTI and the other HTI. Both BISO samples had fairly low merit factors (1600) and both survived. Again, this is the highest exposure that LTI coatings have been tested to. Photomicrographs of these samples are shown in Figs. 3.27 and 3.28.

These results correlate well with the Kaae analytical model (Refs. 5, 8, 9) for TRISO-coated particle performance (see "Analytical Studies of Coating Design" in Task V, Part II). The one sample that failed in P13J (at two temperatures) had the highest calculated tensile stress in the SiC coating of any sample tested in either the P13J capsule or the earlier P13F and P13H capsules.

The metallographic examination revealed an apparent interaction between the carbide fuel and the SiC layer in a few samples after irradiation. An example of this reaction is shown in Figs. 3.17 and 3.18. A comparison of the irradiated P13J samples with those in the P13F and P13H capsules indicates that this reaction is observed only on TRISO-I coatings having buffer thicknesses of 40 μ or less and only at temperatures greater than about 1200°C (avg) for irradiation times longer than 100 days. It has not been observed

Table 3.12
DESCRIPTION AND POSTIRRADIATION EXAMINATION OF COATED PARTICLES TESTED IN CAPSULE P133
(4-Cycle ETR Irradiation)

Lot No.	Type	Coating								Particle				Irradiation Conditions				Postirradiation Examination		Analytical Parameters			Remarks
		Buffer		SiC		Isotropic			Total Thickness (μ)					Temperature (°C)			Fast Dose ^c (10 ²¹ nvt)	Micro Exam ^d (% Fail)	Met Exam ^e (% Fail)	Merit Factor ^f	Shrinkage Rate ^g (1/10 ³ nvt)		
		Thickness (μ)	Density (g/cm ³)	Thickness (μ)	Thickness (μ)	Type ^a	Density (g/cm ³)	Anisotropy Factor		Burnup ^c (X FIMA)	Avg	Max	EOL	SiC	ISO PyC								
3592-35E	SiC barrier	39	1.38	15	10	LTI	(h)	(h)	64	(Th,U)C ₂	150-250	2:1	20	1280	1350	1180 ^c	4.0	1	4	700	(h)	(h)	SiC load-bearing series
3592-37E	SiC barrier	39	1.38	25	10	LTI	(h)	(h)	74	(Th,U)C ₂	150-250	2:1	20	1300	1340	1200	4.0	0	0	1100	(h)	(h)	
3592-45E	SiC barrier	39	1.38	37	8	LTI	(h)	(h)	84	(Th,U)C ₂	150-250	2:1	20	1280	1330	1180 ^c	4.0	0	0	1650	(h)	(h)	
3592-47E	SiC barrier	39	1.38	10	31	LTI	2.03	(h)	80	(Th,U)C ₂	150-250	2:1	20	1180	1300	1110	4.0	0	0	450	550	1	PyC load-bearing series
3592-53E	SiC barrier	39	1.38	10	52	LTI	2.03	1.23	101	(Th,U)C ₂	150-250	2:1	20	1290	1350	1200	4.0	0	0.2	450	900	-2.5	
3592-69E	SiC barrier	51	1.21	29	10	LTI	(h)	(h)	90	(Th,U)C ₂	150-250	2:1	20	1290	1340	1200	4.0	0	0	2000	(h)	(h)	SiC load-bearing series with thicker buffers
3592-71E	SiC barrier	51	1.21	35	10	LTI	(h)	(h)	96	(Th,U)C ₂	150-250	2:1	20	1310	1370	1240	4.0	0	0	2350	(h)	(h)	
3592-35E	SiC barrier	39	1.38	15	10	LTI	(h)	(h)	64	(Th,U)C ₂	150-250	2:1	20	1000	1080	1000 ^c	4.0	6	9	850	(h)	(h)	Intermediate-temperature SiC load-bearing series
3592-37E	SiC barrier	39	1.38	25	10	LTI	(h)	(h)	74	(Th,U)C ₂	150-250	2:1	20	950	1050	950 ^c	4.0	0.7	0	1400	(h)	(h)	
3592-45E	SiC barrier	39	1.38	37	8	LTI	(h)	(h)	84	(Th,U)C ₂	150-250	2:1	20	980 ^c	1050	980 ^c	4.0	0	0	2000	(h)	(h)	
3592-35E	SiC barrier	39	1.38	15	10	LTI	(h)	(h)	64	(Th,U)C ₂	150-250	2:1	20	600	650	600 ^c	4.0	0.2	0	1100	(h)	(h)	Low-temperature SiC load-bearing series
3592-37E	SiC barrier	39	1.38	25	10	LTI	(h)	(h)	74	(Th,U)C ₂	150-250	2:1	20	610 ^c	670	610 ^c	4.0	0	0	1800	(h)	(h)	
3592-45E	SiC barrier	39	1.38	37	8	LTI	(h)	(h)	84	(Th,U)C ₂	150-250	2:1	20	630	690	630 ^c	4.0	0	0	2650	(h)	(h)	
3583-67E	BISO(1300)	51	1.21	--	65	LTI	2.04	1.03	116	(Th,U)C ₂	150-250	2:1	20	1280	1340	1190	4.0	0	0	---	1650	-1	BISO
3592-73E	BISO(2000)	80	1.11	--	38	HTI	2.05	1.04	118	(Th,U)C ₂	150-250	1.6:1	23	1280 ^c	1330 ^c	1190 ^c	4.0	0	0	---	1610	-0.4	

^a LTI denotes low-temperature isotropic coating; HTI denotes high-temperature isotropic coating.

^b All particles are spheroidal and >90% of theoretical density.

^c Estimated.

^d Determined by microscopic examination of approximately 2000 particles.

^e Determined by metallographic examination of 200 to 500 particles.

^f Calculated by method of Schwartz (Ref. 4) using end-of-life conditions and assuming either the SiC or the isotropic PyC to be the load-bearing layer.

^g Shrinkage rate of isotropic PyC coating in parallel direction at total strain of ~22. Estimated from data of Ref. 6.

^h Not determined.

Before Irradiation (100X)

After Irradiation at 1300°C (100X)

After Irradiation at 1300°C (250X)

Fig. 3.17--Photomicrographs of TRISO-I-coated fissile particles (lot 3592-35E) before and after irradiation in Capsule P13J; note apparent reaction between carbide fuel and SiC in some particles after irradiation

After Irradiation at 1000°C (100X)

After Irradiation at 600°C (100X)

Fig. 3.18--TRISO-I-coated fissile particles (lot 3592-35E) after irradiation in capsule P13J

Before Irradiation (100X)

After Irradiation at 1300°C (100X)

Fig. 3.19--TRISO-I-coated fissile particles (lot 3592-37E) before and after irradiation in Capsule P13J

After Irradiation at 1000°C (100X)

After Irradiation at 600°C (100X)

Fig. 3.20--TRISO-I-coated fissile particles (lot 3592-37E) after irradiation in Capsule P13J

Before Irradiation (100X)

After Irradiation at 1300°C (100X)

Fig. 3.21--Photomicrographs of TRISO-I-coated fissile particles (lot 3592-45E) before and after irradiation in Capsule P13J

After Irradiation at 1000°C (100X)

After Irradiation at 600°C (100X)

Fig. 3.22--TRISO-I-coated fissile particles (lot 3592-45E) after irradiation in Capsule P13J

Before Irradiation (100X)

After Irradiation at 1300°C (100X)

Fig. 3.23--TRISO-I-coated fissile particles (lot 3592-47E) before and after irradiation in Capsule P13J; note apparent reaction between carbide fuel and SiC in some particles after irradiation

Before Irradiation (100X)

After Irradiation at 1300°C (125X)

Fig. 3.24--TRISO-I-coated fissile particles (lot 3592-53E) before and after irradiation in Capsule P13J; note apparent reaction between carbide fuel and SiC in some particles after irradiation

Before Irradiation (150X)

After Irradiation at 1300°C (100X)

Fig. 3 25--TRISO-I coated fissile particles (lot 3592-69E) before and after irradiation in Capsule P13J

Before Irradiation (100X)

After Irradiation at 1300°C (100X)

Fig. 3.26--TRISO-I-coated fissile particles (lot 3592-71E) before and after irradiation in Capsule P13J

Before Irradiation (100X)

After Irradiation at 1300°C (100X)

Fig 3.27--BISO-coated fissile particles (lot 3583-67E) before and after irradiation in Capsule P13J

Before Irradiation (100X)

After Irradiation at 1300°C (125X)

Fig. 3.28--BISO-coated fissile particles (lot 3592-73E) before and after irradiation in Capsule P13J

in tests up to 1300°C on TRISO-II coatings (buffer-isotropic-SiC-isotropic) or on TRISO-I coatings with buffers greater than 40 μ . This type of reaction has also been observed in high-temperature TRISO irradiation tests at ORNL and Dragon.

The conclusion drawn from the P13J capsule experiment, together with the earlier P13F and P13H experiments, is that TRISO-coated particles with both LTI and HTI pyrocarbon layers and a wide range of coating geometries have demonstrated very good stability out to full PSC burnup and to about one-half of the full PSC fast-neutron dose. A series of capsule experiments is now being designed (see below) to take these TRISO-coated particles out to or near full exposure. These tests will be conducted at both intermediate and high temperatures to further study the possibility of the intermediate temperatures being more severe.

Capsules P20, P21, and P22. Three additional irradiation experiments are being planned to evaluate reference TRISO-coated particles and to demonstrate their stability under PSC design exposures (Ref. 5). The first two, P20 and P21, will be irradiated concurrently beginning in April (ETR cycle 96). P20 will be irradiated to a fast-neutron dose of about 6×10^{21} n/cm². P21 will be a longer-term test of similar samples designed to achieve a higher fast-neutron dose of about 8×10^{21} n/cm². Design, fabrication, and sample preparation are beginning for capsules P20 and P21. P22 will begin irradiation one cycle later (cycle 97) in June and will also be a long-term test designed for about 8×10^{21} n/cm². All tests will be taken to the full PSC burnup of 20% FIMA.

Capsules FR-1 and FR-2. Two fuel rod irradiation experiments in the ETR are being planned to evaluate the effects of the bonding matrix on coated particle stability at high fast-flux exposure (Ref. 5). Testing of coated particles to date has been on loose particles, and the irradiation program has been designed to guide the development of coated particles that can survive full PSC exposure conditions. Bonding the particles together into a fuel rod may result in additional stresses in the coatings, since the carbonaceous matrix undergoes dimensional changes during fast-neutron irradiation. These fuel rod irradiation tests will be performed to evaluate the stability of the fuel rods and the effects of the fuel rod matrix on coated particle stability.

Capsules FR-1 and FR-2 will test full-diameter, 1-in.-long fuel rods to full burnup of the fissile particles at maximum PSC temperatures and to high fast-flux exposures. Loose coated particles will be tested with each fuel rod for a direct comparison of coated particle performance loose and in the rods.

The present P-capsule design has been modified to accommodate five fuel rods. One fuel rod will be contained in each cell of the capsule, together with the sample of loose coated particles, which will be in a smaller crucible below the fuel rod crucible. Each fuel rod will have a central hole extending half way in from one end so that a central thermocouple can be used to monitor centerline fuel temperatures. Thermocouples will also be included in each loose coated particle sample and in the graphite around the fuel rod.

A number of fuel rod types are candidates for these experiments. Post-irradiation examination of the fuel rods is expected to include dimensional stability, coated particle integrity, interaction of the fuel rods with the graphite containers, and fission-product release tests.

Both experiments are scheduled to begin irradiation in the ETR in June in cycle 97. FR-1 will be irradiated to a fast-neutron dose of 4 to 6×10^{21} n/cm² ($E > 0.18$ Mev), and FR-2, which will test similar samples, will be irradiated to a higher fast dose of 6 to 9×10^{21} n/cm². Design and fabrication of both capsules is in progress.

Capsule G12. Capsule G12 was designed to irradiate specimens from large diameter logs of PSC candidate graphites. Needle-coke graphites including grades H327, 9567 (Nuclear III purity), and TS-688 (EGCR) were tested.

The capsule has completed seven cycles in ETR, with the samples receiving estimated exposures of 3 to 6×10^{21} n/cm² ($E > 0.18$ Mev) at 600° to 1000°C . Postirradiation examination has been completed, and the results are being evaluated.

Capsule G13. Capsule G13, a follow on to G12, contains needle-coke graphites grades H327, 9567 (Nuclear II purity), and NC8 (EGCR), as well as nominally isotropic grade RC4. G13, which is in its fourth ETR cycle, was designed to irradiate specimens from large-diameter extrusions of PSC candidate graphites in the temperature range 600° to 1050°C to a maximum dose of about 5 to 6×10^{21} n/cm² ($E > 0.18$ Mev). Approximately half of this maximum dose has been accumulated, with the capsule and instrumentation still functioning satisfactorily.

Capsule G14. Capsule G14, a follow on to G13, will contain proof test samples of the candidate graphite selected for the PSC core.

Graphite Irradiation Data from Other Programs. In addition to the above PSC experiments, data also have been utilized from programs at other laboratories. These programs were directed toward studies to measure the dimensional changes, physical property changes, and irradiation behavior of a number of advanced graphites. Some of these experiments presently are being conducted concurrently with the PSC program. They were designed primarily to study the mechanisms of irradiation damage at high temperatures and doses for other reactor programs. Several of the PSC candidate materials also appear as a small part of these programs.

The current status of high-temperature irradiation experiments that include needle-coke graphites is shown in Table 3.13. Graphites H327 and 9567 are of particular interest to the PSC program. The other graphites are needle-coke materials that are under irradiation on other programs. EGCR graphite (NC8 or TS-688) has been considered as a standard or reference material against which other candidate core graphites have been compared. The combinations of planned irradiation experiments which have provided or can provide PSC core end-of-life exposures in the range 900° to 1000°C on graphites H327 and 9567 are presented in Table 3.14. The PSC end-of-life dose (approximately 8×10^{21} n/cm²) has already been achieved on graphites NC8 (EGCR) and 9567 (Nuclear III) for exposure temperatures between 925° and 1075°C . These experiments were conducted in the GETR by BNWL.

Table 3.13
IRRADIATION TESTS OF PSC CANDIDATE GRAPHITES AND OTHER NEEDLE COKE GRAPHITES
(Experiments Completed, Underway, or Planned)

Capsule No.	Material	Temp (°C)	Total Dose (10^{21} n/cm ²)	Date Data Available	Properties Measured ^a
Experiments conducted by Gulf General Atomic or jointly with BNWL, ORNL, or UK					
G10	H-283 711-TS TS-688 (EGCR) CHN	1000-1200 1000-1200 1200 450-1400	1.2-3.3 1.2-3.3 1.2-3.3 1.2-3.3	Completed Completed Completed Completed	Dimensions, thermal conductivity, coefficient of thermal expansion
G11	H-327 9567 (N III) TS-688 (EGCR)	500-600 500-600 500-600	1.0-1.7 1.0-1.7 1.0-1.7	Completed Completed Completed	Dimensions
G12	H-327 9567 (N III) TS-688 (EGCR)	885-900	5.0-5.6	Completed	Dimensions, thermal conductivity, coefficient of thermal expansion
G13	H-327 9567 (N II) NC-8 (EGCR)	600-1050	5.0-6.0	10-68	Dimensions, thermal conductivity, coefficient of thermal expansion, mechanical properties, elastic moduli
G14	H-327	600-1050	5.0-6.0	1-70	Dimensions, thermal conductivity, coefficient of thermal expansion, mechanical properties, elastic moduli
GE-H-13-13	TS-688 (EGCR)	800-1250	~8.0	Completed	Dimensions
GE-H-13-14	H-283 711-TS-N	950-1230 950-1230	4.0-4.5	Completed	Dimensions
GE-H-13-16	H-283 711-TS-N	950-1230 950-1230	7.0-7.5	Completed	Dimensions
Dragon	TS-688 (EGCR) 9567 H-327	900-1100	~2.0	3-68	Dimensions and creep
DFR-I	H-327 9567 NC-8 (EGCR) H-327 9567 NC-8 (EGCR) 711-TS-N	600-700 900-1100	~9.0	(b)	Dimensions and coefficient of thermal expansion
DFR-II	H-327 CHN	900-1100	~18.0	(b)	Dimensions and coefficient of thermal expansion
Piggyback samples in miscellaneous P-capsules and Peach Bottom proof test elements	H-327 9567 (N II) TS-688 (EGCR)	600-1000	1.3-4.1, 6-9	1-70	Dimensions, thermal conductivity, coefficient of thermal expansion
Miscellaneous capsules	TS-688 (EGCR)	400-800	~16.0	Completed	Dimensions
	TS-688 (EGCR) 9567	1000	~9.0	Completed	Dimensions
	TS-688 (EGCR) 9567	1000-1400	~4.0	Completed	Dimensions
	H-327				
Other experiments conducted by BNWL; results made available to Gulf General Atomic					
GE-H-13-14	H-327 9567	800-1250	0.5-2.0	Completed	Dimensions
GE-H-13-16	H-327 9567	800-1250	2.0-5.5	Completed	Dimensions
GETR capsules	9567 NC-8 (EGCR)	925-1075	~11.4	Completed	Dimensions
Other experiments conducted by ORNL; results made available to Gulf General Atomic					
---	TS-688 (EGCR)	300-1000	~1.0	3-68	Creep

^a All measurements made in parallel and perpendicular directions except thermal conductivity which was measured in perpendicular only.

^b DFR reactor shut down and irradiation schedule still indefinite.

Table 3.14

COMBINATIONS OF IRRADIATION EXPERIMENTS TO PROVIDE PSC CORE LIFETIME EXPOSURE

Capsule No.	Specimens Delivered	Date Data Available	Temp (°C)	Total Dose (10^{21} n/cm ²)	Reactor
G10	---	Completed	1000-1200	---	ETR
GE-H-13-14	---	Completed	1000-1200	---	ETR
GE-H-13-16	---	Completed	1000-1200	7 -9	ETR
G12	---	Completed	885-990	---	ETR
GE-H-13-17	1-68	7-68	885-1000	7.5-8.1	ETR
G12	---	Completed	885-990	---	ETR
G14	9-68	7-69	885-1000	10.0	ETR
G13	---	10-68	600-1050	---	ETR
G14	10-68	9-69	600-1050	10.0	ETR
DFR-I	---	Indefinite	900-1100	9.0	DFR
BNWL capsules ^a	---	7-67	925-1075	11.4	GETR

^a
9567 only.

Effect of Irradiation on Graphite Properties A survey has been performed for dimensional change data presently available on irradiated graphites that are candidates for PSC reactor-core applications. Anisotropic, needle-coke graphites have been primary candidates during the past year or so, with the bulk of the data being available for EGCR-grade graphites; smaller amounts of data have been reported for grades H327 and 9567 (Nuclear III). The dimensional change data on these graphites have been compiled, and the observations summarized briefly below.

The behavior of the other two needle-coke graphites seems to be close to that of the EGCR graphites, with minor variations. The EGCR grade graphite exhibits a consistent divergence of the curves for the two orientations within each temperature interval. The perpendicular orientation shows only contraction within the exposure life of the PSC core (approximately 8×10^{-4} n/cm²) through 1400 F. A minimum in the curve (or a "turnaround") in the perpendicular orientation curve first appears in the 1200° to 1400° F range at about 9×10^{-4} n/cm². The minimum shifts toward lower exposure values with increase in temperature, apparently stabilizing at about 5.5×10^{-4} n/cm² for temperatures of 1600 F and higher, and apparently begins to drop (larger contractions) with successively higher temperatures to 2400° F. The values for $\Delta L/L$ in the perpendicular orientation first appear to become positive within the PSC core exposure life in the 1400° to 1600° F interval. The slope of the expansion leg of the perpendicular curves increases with increase in temperatures and becomes moderately steep at temperatures above 2000° F.

The parallel orientation curves show contraction for all temperatures reported. A knee, or significant change in slope, first appears within PSC core exposure life in the 1400° to 1600° F range and shifts rapidly toward lower exposure values with relatively small additional increase in temperature.

General observations and comparisons of all three candidate graphites for each temperature interval and orientation have been summarized in Table 3.15. Although these three candidate graphites exhibit an estimated maximum of 3% to 5% expansion in the perpendicular (log radial) direction within PSC core lifetime, such expansion occurs only in a range of temperatures above about 1550° F (850 C). Thus, the over-all behavior of all three graphites appears to be satisfactory for the PSC core.

The effect of irradiation on the elastic modulus of the candidate graphites is illustrated in Figs. 3.29 and 3.30. Initially the elastic modulus increases abruptly (by a factor of about two) to a dose between 2 and 4×10^{21} n/cm² ($E = 0.18$ Mev); beyond this dose no significant change appears to occur. An experiment to determine the effect of an exposure of about 5×10^{21} n/cm² ($E = 0.18$ Mev) on grade H327 is now in progress, with the specimens presently being irradiated in Gulf General Atomic capsule G13 in the ETR.

The effect of irradiation on the other mechanical properties of the candidate grades of graphite has not been measured in Gulf General Atomic experiments, nor has it apparently been reported by other laboratories. An experiment has been included in Gulf General Atomic capsule G13 to provide data for grades H327 and 9567 (Nuclear II) on the modulus of rupture and the modulus of elasticity (in flexure) and on the ultimate compressive stress.

Fig. 3.29---Young's modulus of ECCR graphite (parallel) as a function of exposure

Fig. 3.30--Young's modulus of EGCR graphite (perpendicular) as a function of exposure

Table 3.15

COMPARISON OF DIMENSIONAL CHANGE DATA FOR IRRADIATED CANDIDATE CORE GRAPHITES

Temp Range (°F)	Remarks
600-800	Parallel: Data on EGCR. Perpendicular: Data on EGCR. Slightly lower rate of contraction with exposure than for parallel orientation.
800-1000	Parallel: Some data on H327 and 9567, more on EGCR; EGCR contracts continually through 10×10^{21} nvt. H327 contracts at somewhat higher rate than EGCR, with 9567 curve about half way between them. Perpendicular: No data on H327 and 9567; EGCR data contracts continuously (at about the same rate as for parallel orientation) to $4\frac{1}{2} \times 10^{21}$ nvt exposure; beyond this point perpendicular orientation contracts progressively less than parallel, reaches a minimum at $8\frac{1}{2} \times 10^{21}$ nvt, and begins to show net expansion at 11 to 12×10^{21} nvt.
1000-1200	Parallel: Data for H327 and 9567 show near-coincidence for these two grades out to $2\frac{1}{2} \times 10^{21}$ nvt. EGCR contracts at slightly lower rate than that for the other two grades and contracts continuously through 10×10^{21} nvt. Perpendicular: Data limited for H327 and 9567, curves nearly coincident with that for EGCR (which contracts at lower rate than EGCR curve for parallel orientation, reaches a minimum at $8\frac{1}{2} \times 10^{21}$ nvt, and begins to show net expansion at 11 to 12×10^{21} nvt).
1200-1400	Parallel: Data for H327 and 9567 show near-coincidence for these two grades out to $2\frac{1}{2} \times 10^{21}$ nvt. EGCR contracts at slightly lower rate than that for the other two grades and contracts continuously through 10×10^{21} nvt. Perpendicular: Data limited for H327 and 9567, curves nearly coincident with that for EGCR (which contracts at lower rate than EGCR curve for parallel orientation, reaches a minimum at $8\frac{1}{2} \times 10^{21}$ nvt, and begins to show net expansion at 11 to 12×10^{21} nvt).
1400-1600	Parallel: No data for 9567, and almost none for H327. EGCR curve contracts continuously at approximately constant rate out to 8×10^{21} nvt, beyond which contraction rate accelerates.

Table 3.15 (continued)

Temp Range (°F)	Remarks
1600-1800	Perpendicular: No data for 9567, and almost none for H327. EGCR curve contracts at lower rate than for EGCR parallel orientation, reaches a minimum at $6\frac{1}{2} \times 10^{21}$ nvt and begins to show net expansion at $9\frac{1}{2} \times 10^{21}$ nvt. Parallel: Curves for grades H327 and 9567 essentially coincident through 5×10^{21} nvt. EGCR curve near coincidence with those for H327 and 9567 initially and through 4×10^{21} nvt (beyond which EGCR curve diverges to contract at slightly lower rate than the other two grades).
1800-2000	Perpendicular: Curves for all three grades essentially coincide initially and through 3×10^{21} nvt (beyond which some divergence occurs, but all three begin to show net expansion at 6×10^{21} nvt). The EGCR curve reaches the lowest minimum value of the three grades at 5×10^{21} nvt; H327 shows next-lowest minimum value at 4×10^{21} nvt; and 9567 shows the highest minimum value in contraction at 4×10^{21} nvt. The H327 and 9567 curves lie fairly close together. The curve for 9567 perpendicular extends well into the expansion region, climbing at an accelerating rate to attain about 6% net expansion at an exposure of 8.6×10^{21} nvt (approximately PSC lifetime dose). Parallel: Curves for H327 and 9567 coincide initially and through 6×10^{21} nvt, whereas curve for EGCR contracts at a slightly lower, steady rate up to $6\frac{1}{2} \times 10^{21}$ nvt (beyond which the contraction rate accelerates rapidly with additional exposure).
2000-2200	Perpendicular: Contraction curves show much lower rate than for parallel orientation. The H327 and 9567 curves run close together, both reaching the base line of dimensional change at 6×10^{21} nvt and showing minimum values at 4×10^{21} nvt; (the 9567 curve shows slightly less contraction than that for H327). The EGCR curve coincides initially with that for 9567, but continues contracting to reach a minimum value (with greater contraction) at 5×10^{21} nvt, reaching the base line at 7×10^{21} nvt, a slightly higher exposure. Parallel: The curves for H327 and 9567 lie close together out to 6×10^{21} nvt, the H327 curve showing a slightly higher contraction rate than that for 9567. The EGCR curve lies somewhat apart from the other two and at a somewhat lower rate of contraction than that for 9567.

Table 3.15 (continued)

Temp Range (°F)	Remarks
2200-2400	Perpendicular: Contraction curves show much lower rate than for parallel orientation. The H327 and 9567 curves run close together, both reaching the base line of dimensional change at 6×10^{21} nvt and showing minimum values at 4×10^{21} nvt; (the 9567 curve shows slightly less contraction than that for H327). The EGCR curve coincides initially with that for 9567, but continues contracting to reach a minimum value (with greater contraction) at 5×10^{21} nvt, reaching the base line at 7×10^{21} nvt, a slightly higher exposure. Parallel: The curves for H327 and 9567 lie close together out to 6×10^{21} nvt, the H327 curve showing a slightly higher contraction rate than that for 9567. The EGCR curve lies somewhat apart from the other two and at a somewhat lower rate of contraction than that for 9567. Perpendicular: Data for all three grades somewhat limited, but the three curves lie fairly close together, with H327 showing slightly more contraction than 9567 or EGCR (which reach a minimum at 3×10^{21} nvt).

A survey of the thermal conductivity data of irradiated anisotropic graphites measured perpendicular to the extrusion direction has been made and combined with the data on 780-S (a Texas-coke grade irradiated in capsule G9). These results are shown in Fig. 3.31, in which irradiated thermal conductivity values have been plotted as a function of test and irradiation temperature in the service range of the PSC graphite (400° to 1100°C). All irradiation temperatures were measured with thermocouples (accurate to $\pm 100^\circ\text{C}$ above 800°C and $\pm 30^\circ\text{C}$ below 800°C). A description of each graphite included in this survey has been given in Table 3.16. Samples from capsules G10, G11, and G12 have been measured after the above survey and will be reported when a second survey that is now in process has been completed. Additional data are available for the latter survey at higher doses (about 7 to 8×10^{21} n/cm²) in the range 900° to 1100°C from capsules GE-H-13-12, -13, -14, and -16, which have been constructed and irradiated by Battelle Northwest Laboratories.

The data points in Fig. 3.31 were obtained either directly from the original curves of thermal conductivity as a function of test temperature, if the test temperature coincided with the irradiation temperature, or by a short extrapolation of the data when the maximum test temperature was lower than the irradiation temperature (see tabulation in Fig. 3.31). Serious error is not expected to result from short extrapolation of the Battelle data.

Fig. 3.31--Thermal conductivity of needle coke and Texas coke graphites measured in the perpendicular direction as a function of irradiation temperature (thermal conductivity measured at irradiation temperatures)

Thermal annealing below the irradiation temperature during measurement is considered to be insignificant and should have no effect on the thermal conductivity values. The data (Fig. 3.31) indicate little or no dependence on irradiation temperature over the range 400° to 1100°C, the range of temperatures for graphite service in PSC. Two points were available at higher doses: 3.3 and 14.2 x 10²¹ n/cm². The dashed lines drawn through these points provide an approximate basis for PSC design at higher doses. Figure 3.32 is a plot of the data in the range 625° to 775°C as a function of dose.

Table 3.16
DESCRIPTION OF GRAPHITES IN THERMAL CONDUCTIVITY DATA SURVEY

Material	Log Size (in.)	Filler Coke	Density (g/cm ³)	Description
CSF	4x4	Cleves	1.66	F, purified at 2700°C.
NC-7 (EGCR)	16-3/8x 16-3/8	Needle	1.70	Electrode stock similar to graphite used for NC-8 (EGCR)
TSX	6x6	Needle	1.71	Graphitized at 3000°C.
780-S	4	Texas	1.80	Fine-grained graphite, pitch impregnated, graphitized at 2800°C.

CHEMISTRY

The objectives of this subtask are to determine:

1. The release behavior of solid fission products that might be important plateout sources and gaseous fission products from the fuel particles being considered for use in the plant. Release behavior will be determined as a function of time, temperature, and irradiation by means of analysis and postirradiation annealing and in-TRIGA furnace tests on irradiated fuel particles. The data will be used to determine the effects of such variables as particle diameter, coating thickness, and coating structure and as input for the calculation of fission-product release to the coolant circuit. The particles used in these tests will be irradiated under other tasks of this program or will be obtained from other sources.
2. The sorption and diffusion behavior of solid fission products in graphites, being considered for use in the plant, as a function of temperature and concentration by means of analysis and out-of-reactor chemical experiments on graphite samples doped with "fissium."
3. The deposition (sorption) behavior of metallic fission products on coolant circuit metals and the relative distribution of fission products between carbon dust and metal surfaces.

Fig. 3.32--Thermal conductivity (perpendicular) as a function of dose at 625° to 775°C

4. The extent and effects of oxidation attack on both fuel particles and fuel element graphite as the result of steam leakage into the coolant circuit.

Fission-product Release

Release of Iodine from Cracked Particles. A study of the release of iodine from cracked irradiated particles has been conducted to provide information for the safety analysis of the Fort St. Vrain reactor. The objective of the study was to measure the iodine release from particles as a function of burnup and to obtain a mass balance by also measuring the retention of iodine in the particles. The results are given in Table 3.17.

The experimental procedure and the results of the iodine release measurements were presented in the previous quarterly report. The data, presented in Table 3.17, demonstrates that the release of iodine from cracked particles is equal to or less than the amount that should be released by fission-product recoil from uncoated kernels (approximately 5%). The iodine release behavior is similar to that exhibited by the inert gaseous fission products in the temperature range 1000° to 1300°C.

To confirm the measured release fractions of iodine from the particles, it was necessary to obtain a material balance for the iodine produced by showing that the balance of the iodine was retained in the particles. In this phase of the experiment an oxidative destruction technique was used to release the Xe daughter of the I retained in the particles. The particles were allowed to decay for 24 hr after annealing, and the entire sample was then burned at 1000°C in a 10% oxygen/90% helium atmosphere. Gases released in the burning operation were collected in a liquid-nitrogen-cooled charcoal trap. The Xe^{135} disintegration rate in the trap was determined using a NaI crystal detector.

The total Xe^{135} (the amount present in the oxidation gas trap plus smaller amounts released to the irradiation, anneal, and purge traps used earlier in the experiment) was used to calculate the concentration of iodine and xenon present at various times during the experiment. The expressions employed in these calculations were those given by Friedlander, Kennedy, and Miller (Ref. 10). The iodine concentrations were calculated by the relation

$$N_1 = \frac{R_1[1 - \exp(-\lambda_1 t_b)]}{\lambda_1} ,$$

where N_1 = the number of atoms of I^{135} , R_1 = the formation rate of I^{135} , λ_1 = the decay constant for I^{135} , and t_b = the irradiation time. Since the I^{135} concentration in these experiments was determined from the Xe^{135} daughter, it was also necessary to know the Xe^{135} contents. The expression for the Xe^{135} concentrations at various times after the irradiation is (Ref. 10)

$$N_2 = \left\{ \frac{R_1}{\lambda_2} [1 - \exp(-\lambda_2 t_b)] + \frac{R_1}{\lambda_1 - \lambda_2} [\exp(-\lambda_1 t_b) - \exp(-\lambda_2 t_b)] \right\}$$

$$\exp(-\lambda_2 t_s) + \frac{R_1}{\lambda_2 - \lambda_1} [1 - \exp(-\lambda_1 t_b)] [\exp(-\lambda_1 t_s) - \exp(-\lambda_2 t_s)] ,$$

Table 3.17

IODINE RELEASE FROM UNCOATED AND CRACKED PARTICLES

Sample No.	Kernel	Coating	Burnup (% FIMA)	Annealing Temp ^a (°C)	Fractional Release of Iodine	Fractional Retention of Iodine in Particles	Mass Balance (%)
P1701ESR-1	150- to 250- μ (Th,U)C ₂ Th:U=1.6:1	Uncoated kernel	0	1000	0.008 ^b	0.96	97
3031-38E	177- to 250- μ (Th,U)C ₂ Th:U=4.2:1	40- μ buffer 65- μ isotropic	4	1000	0.04	0.58 ^c	62
3023-77E	177- to 250- μ (Th,U)C ₂ Th:U=4.2:1	29- μ buffer 68- μ isotropic	7.9	1000	0.02	0.96	98
2814-149E	150- to 250- μ (Th,U)C ₂ Th:U=1.6:1	25- μ buffer 80- μ isotropic	14.1	1000	0.01	0.83	84
2824-9E	150- to 250- μ (Th,U)C ₂ Th:U=1.6:1	57- μ buffer 41- μ isotropic	24	1000	0.03	0.98	101
2824-9E	150- to 250- μ (Th,U)C ₂ Th:U=1.6:1	57- μ buffer 41- μ isotropic	24	1300	0.04	1.03	107

^aTime at temperature was 6 hr.

^bThis fractional release value seems unreasonably low, since approximately 5% of the iodine should be lost by recoil from uncoated fuel.

^cLow retention of iodine resulted from loss of Xe¹³⁵ daughter due to hydrolysis of cracked particles.

where N_2 = the number of atoms of Xe^{135} , t_p = the irradiation time, t_s = the time interval between the irradiation and the determination time, λ_1 = the decay constant for I^{135} , λ_2 = the decay constant for Xe^{135} , and R_1 = the formation rate of I^{135} . The fractional retentions of iodine by the particles were calculated by solving this equation using the RADDAG code and relating the calculated Xe^{135} concentrations to those measured in the oxidation traps at the time of destruction. This relation is valid because the Xe^{135} present in the particles was formed only by the decay of the I^{135} precursor.

Two samples (lots 3031-38E and 2814-149E) showed low retentions of iodine that might be interpreted as indicating an unobserved loss of iodine during the annealing tests (Table 3.17). However, microscopic examination of the sample from lot 2814-149E after the annealing test showed that these particles suffered some hydrolysis damage. The kernel and coating of one of the particles had completely ruptured. Since all cracked particles were stored under an argon atmosphere until they were annealed for iodine release, air hydrolysis is thought to have occurred after the particles were removed from the TRIGA furnace. The low release of iodine during the anneal test indicates that the particles were intact (with hairline cracks) at that time. The sample from lot 3031-38E also showed a low retention of iodine. The high concentration of thorium (Th:U = 4.2:1) made this sample much more susceptible to hydrolysis. The data thus indicate that the low retention of iodine in both cases resulted from a loss of Xe^{135} daughter owing to the hydrolysis of the particles between the annealing and the oxidation steps rather than from a loss of iodine during the anneal.

The excellent mass balances obtained in the remaining experiments show that the measured iodine releases are accurate and that the remaining iodine is retained in the particles and is not lost by some unobserved or unknown mechanism. Thus it can be concluded that cracked irradiated fuel particles release a small fraction of their iodine in 6 hr.

Fission-product Release Data for Irradiated TRISO-coated Particles.

Measurements were made of the fission-product release of SiC-coated particles irradiated in the P13F capsule. The objective was to determine the effect of high fuel particle burnup on the fission-product retention characteristics of TRISO-coated particles. The release behavior was determined at 1400°, 1600°, and 1800°C by the postirradiation release method on particles from lots 3332-101E, 3332-131E, and 3332-145E. These particles withstood fuel burnups of approximately 12% FIMA with a very high retention of metallic fission products.

Approximately 90 high-burnup particles (30 particles in each of three samples) from each lot were leached, weighed, and irradiated to 30 Mw-hr in the TRIGA Mark III reactor. The samples were stored for approximately seven days to permit decay of short-lived activities. They were then gamma-counted on the lithium-drifted germanium crystal to determine the La^{140} present. The La^{140} counts were used to calculate the total number of fissions produced in each sample during the TRIGA irradiation. This number of fissions was comparable to that calculated from the uranium contents, the TRIGA Mark III neutron flux, and the TRIGA irradiation time. The samples were then annealed at 1400°, 1600°, and 1800°C for 20 hr, and the release of Ba^{140} was measured.

Strontium-90, most of which was produced in the P-capsule irradiation, was the only strontium isotope present in these high-burnup samples that could be used for release studies (9.7-hr Sr^{91} , produced in the TRIGA activation, decayed out during the seven-day storage period). In-pile release measurements conducted previously indicated a very low release of Sr^{90} during the P13F capsule irradiation at 900° and 1300°C, indicating that essentially all the Sr^{90} remained in the particles. The Sr^{90} analyses of the graphite collector samples from the annealing tests were conducted radiochemically.

The barium and strontium release data are summarized in Table 3.18. Xenon release data obtained during the anneals indicate that no significant failure of coatings occurred during the annealing tests. If both the SiC and the outer pyrolytic carbon coatings had failed, gross xenon release would have occurred. The in-pile release data on these particles obtained during the P13F capsule irradiation clearly show that the TRISO-coated particles retained metallic fission products to a high degree during the capsule irradiation (to burnups in excess of 10% FIMA) and that the SiC-barrier layers on these samples were intact when the capsule was removed from the reactor.

All three samples showed good retention of metallic nuclides at 1400°C regardless of the temperature of irradiation. However, particles from lot 3332-131E showed relatively high metallic release at 1600°C and particles from lots 3332-131E and 3332-145E showed relatively high metallic release at 1800°C, showing evidence of some particle failure. A comparison of the data in Table 3.18 with the results of postactivation release measurements on unirradiated samples from these lots (see Task V, Part II of this report) shows a higher degree of failure in the irradiated samples from lots 3332-131E and 3332-145E than in the unirradiated samples from these two lots.

Particles from lot 3332-131E appeared to be less desirable initially than were particles from the other lots, either because of high contamination levels in the outer isotropic pyrocarbon coatings or because of imperfect SiC layers. Even unirradiated samples of this lot showed relatively high metallic-fission-product release, especially at the higher temperatures where thermally activated processes can be expected to be most damaging to the SiC coating.

It would appear that initial poor quality, temperature, irradiation, or the synergistic effects of all three can lead to decreased ability of TRISO particles to withstand high temperatures without failure. There is no reason to expect that irradiation alone affects the retention characteristics of the SiC coatings. However, irradiation can enhance the rate at which a damaging thermally activated process occurs and thereby lead to coating failure, especially at higher temperatures. This effect is expected to be most pronounced for particles which are initially of poor quality.

Particles in lot 3332-101E, which proved superior to the other samples, showed the ability of TRISO particles to retain fission products under conditions of high temperature and high burnup. Furthermore, since all the samples showed low in-pile release and survived the 1400°C laboratory anneal, they behaved satisfactorily from the standpoint of the PSC reactor. This test is conservative because in the reactor only a few percent of the fuel particles will operate at temperatures above 1000°C.

Table 3.18

FISSION-PRODUCT RELEASE OF IRRADIATED SiC FUEL PARTICLES

Lot No. & Particle Description ^a	Coating Thickness (μ)				Irrad. Cond.		Anneal		Fractional Release	
	Buffer	SiC	Iso	Total	Temp (°C)	Burnup (% FIMA)	Temp (°C)	Time (hr)	Ba ¹⁴⁰	Sr ⁹⁰ ^b
3332-101E (Th,U)C ₂ Th/U=3.0 150-250 μ	58	13	40	111	1300	10.5	1400	20	3.3×10^{-3}	9.9×10^{-5}
							1600	20	3.2×10^{-4}	5.3×10^{-4}
							1800	20	1.6×10^{-4}	6.6×10^{-4}
					900	13.2	1400	21	$< 1.4 \times 10^{-4}$	---
							1600	21	3.6×10^{-4}	---
							1800	20	4.0×10^{-4}	8.3×10^{-5}
3332-131E (Th,U)C ₂ Th/U=3.0 150-250 μ	34	30	10	74	1300	11.6	1400	20	4.2×10^{-5}	---
							1600	20	4.1×10^{-2}	2.0×10^{-2}
							1800	20	4.8×10^{-1}	6.2×10^{-1}
3332-145E (Th,U)C ₂ Th/U=3.0 150-250 μ	34	40	9	83	1300	12.6	1400	20	$< 4.2 \times 10^{-6}$	---
							1600	20	$< 1.2 \times 10^{-5}$	---
							1800	21	2.1×10^{-1}	2.6×10^{-1}
					900	13.2	1400	20	$< 3.3 \times 10^{-6}$	---
							1600	20	$< 3.6 \times 10^{-5}$	---
							1800	20	2.3×10^{-1}	1.8×10^{-1}

^aAll samples were leached before activation in TRIGA.

^bThe release of Sr⁹⁰ constitutes postirradiation anneal rather than a postactivation anneal (see text).

Volatilization of Fission Products from the Core During a High-Temperature Excursion. Three additional simulated core heatup experiments have been performed specifically to aid in determining the release of radioiodine from the PCRV during a loss-of-coolant accident. The results of previous core heatup experiments are described in earlier quarterly reports (GA-8270, GA-8038, and GA-7939).

Two of the present experiments were performed in the King furnace apparatus described in a previous quarterly report (GA-8038). The other experiment used a new apparatus, a one-twelfth-scale simulation of the upper reactor plenum and core barrel. These new tests were expected to yield the factor of conservatism to be applied to the results of the previous core heatup experiments in which a maximum of 5.5% iodine release was observed.

The previous King furnace tests are believed to be conservative because the graphite heater tube that contained the sample was attached directly to the exit pipe. Effusing fission products, therefore, were efficiently purged to the filter-trap assembly, with little chance for diffusion, dilution, and plateout inside the furnace.

For these new experiments, the King furnace system was modified to better simulate the geometry of the reactor and the flow conditions therein. Three basic changes were made: (1) A steel liner was installed inside the furnace dome 0.5 in. from the water-cooled top, bottom, and sides. This innovation simulated the thermal barrier in the reactor and provided a hot metal surface on which iodine could react to form the less volatile FeI_2 . (2) An exit tube that penetrated the steel liner but did not communicate directly with the heater tube was provided. The outlet end of the graphite heater tube was 1.0 in. from the inlet of the steel exit pipe. (3) The purge flow path in the furnace was not directed down the heater tube. That is, the sample was not swept with the entire purge gas as in the previous experiments. Moreover, the flow rate was adjusted to 100 cm^3/min , which is a direct volume scaledown of the maximum accidental N_2 purge rate in the reactor.

For these tests, a modified "May pack" was used as the iodine collection trap. The May pack is a series of adsorbents and filters that aid in identifying the trapped species. The May pack in these tests consisted of (in order) a copper gauze trap for collection of elemental iodine, a millipore filter for removal of particulates, a room-temperature activated-carbon trap for adsorption of volatile iodine species (such as methyl iodide), and, finally, a -78°C silica gel trap for final trapping of volatile forms. (It is expected that the species CH_3I will be formed to a negligible extent inside the PCRV because of the extreme temperatures involved.)

For the King furnace experiments, an iodide mixture (BaI_2 , SrI_2 , CsI) was used as the source of iodine for the first test (Corex 13) and elemental I_2 vapor for the second (Corex 14). Thus, the two extremes of iodine volatility were utilized. It was expected that the use of elemental iodine would yield the highest fractional releases and would therefore be very conservative. The conditions for each experiment are given in Table 3.19 and are discussed in detail separately below.

Table 3.19
CONDITIONS OF KING FURNACE CORE HEATUP EXPERIMENTS

	Corex 13	Corex 14
Iodine injection mode	CsI, SrI ₂ , BaI ₂ (0.92 g I)	18 g I ₂ vapor
Run length, hr	47	48
Max graphite temp, °C	2000	2000
Outer radiation shield temp, °C	900	900
Steel liner temp range, top, °C	604-771	621-782
Steel liner temp range, sides °C	532-654	399-627
Steel liner temp range, bottom, °C	266-588	427-782
Exit pipe temp range, °C	250-25	250-25

In the first experiment (Corex 13), the graphite heater tube was first loaded with fission product metals by heating 9 g of fissium* mixture in a graphite crucible (held in the heater tube) to 2000°C for 3 hr. This procedure ensured that the iodine species, formed by dissociation of the iodide source material, would have ample opportunity to react with metals before effusing from the hot graphite, as in the reactor situation

The source material in the first experiment consisted of 0.725 mM BaI₂, 1.08 mM SrI₂ and 3.62 mM CsI, the three metals being in the proper molar ratios for an equilibrium core. This yielded a total of 0.92 g of iodine, which is a direct volumetric scaledown of the reactor (volume scaling factor = $3.3 \text{ ft}^3 / 3.1 \times 10^4 \text{ ft}^3 \sim 10^{-4}$). Thus, the volumetric iodine concentration for both the reactor and the Corex 13 experiment, assuming all of the released iodine is in the gas phase, is about 0.3 g/ft³. The mixture was tagged with one mc of I¹³¹ (by dissolution and precipitation) and then loaded into a sealed graphite crucible attached to a graphite rod. The sample was slowly pushed into the hot zone of the heater tube, after the furnace had been brought to 2000°C. This technique allowed the volatile iodide species to be released into the furnace volume over a period of several hours, which is also the case for the reactor accident.

In the second experiment (Corex 14), 18 g of elemental iodine was injected into the hot zone of the furnace by passing helium at 25 cm³/min over a heated iodine source and into the furnace through a mullite tube. This amount of iodine (18 g) is a direct surface area scaledown of the reactor

* Fissium is a mixture of important fission product metals plus uranium and thorium in the proper molar ratio, corresponding to a six-year HTGR core.

upper plenum and core barrel surface area as compared to the King furnace liner surface area (surface area scale factor = $12.3 \text{ ft}^2/5967 \text{ ft}^2 \approx 2 \times 10^{-3}$). Thus, the average iodine surface loading for both the Corex 14 experiment and the reactor case is 1.5 g/ft^2 , or 1.7 mg/cm^2 .

The conditions of the Corex 14 test were therefore very conservative, because the scaling factor takes no credit for plateout in the lower plenum or for reduction of volatile iodine content by reaction with fission-product metals.

At the end of the tests the furnace was dismantled and all internal parts were radioassayed. The heater tube, radiation shields, exit pipe, and traps were scanned through a 1.0-in. collimator using a 3-in. NaI scintillation crystal. These activity scans were compared with scanning standards previously prepared and calibrated. The steel liner was cut into 4-in. strips, and these were counted. Calibrated standards counted at the same geometry were used. The furnace dome, base plate, and electrodes were wiped and the wipes were counted. The results of both tests are given in Table 3.20.

Table 3.20
FRACTIONAL DISTRIBUTION OF IODINE IN KING FURNACE PARTS

	Corex 13 (Iodide Source)	Corex 14 (Iodine Source)
Copper gauze trap	3.4×10^{-5}	1.3×10^{-4}
Millipore filter	3.1×10^{-5}	2.1×10^{-5}
Charcoal trap, 25°C	1.0×10^{-6}	1.0×10^{-4}
Silica gel, -78°C	None	6.1×10^{-5}
Exit pipe, 0 to 6 in., 250° → 50°C	2.4×10^{-3}	7.0×10^{-3}
Exit pipe, 6 to 12 in., 50° → 25°C	Negligible	9.7×10^{-4}
Exit pipe, 12 to 22 in., 25°C	Negligible	Negligible
Steel liner top	9.9×10^{-5}	2.0×10^{-2}
Steel liner sides	2.6×10^{-2}	9.0×10^{-2}
Steel liner bottom	1.3×10^{-4}	1.4×10^{-2}
Heater tube diffusion plugs and collets	7.9×10^{-2}	5.7×10^{-2}
Radiation shields, 500° to 1900°C	Negligible	Negligible
Radiation shield holder, 300° to 500°C	Negligible	8.6×10^{-3}
Iodine remaining in generator	Negligible	1.7×10^{-4}
Furnace dome, water-cooled	4.8×10^{-1}	2.0×10^{-1}
Electrodes, Ni-plated, water-cooled	1.4×10^{-1}	1.7×10^{-1}
Copper base plate, water-cooled	4.2×10^{-2}	$\sim 3 \times 10^{-1}$
Total found	0.77	0.86

A schematic of the apparatus for the one-twelfth-scale reactor plenum simulation test is shown in Fig. 3.33. The apparatus was a simulation of the upper plenum of the PSC reactor including the access penetration. For this test it was assumed that the access penetration (the largest in the PCRV upper head) had failed, resulting in the loss-of-coolant accident. Since the scale was one-twelfth, the inside diameter of the vessel liner was 31 in. Eight sheets of 0.020-in. mild steel, separated by 1/16-in. wires, constituted the upper thermal barrier. Seven sheets of steel lined the side walls of the vessel.

The temperature of the simulated top reflector surface was maintained to simulate the time-temperature history of a core-heatup accident as shown in Fig. 3.34. The thermal barrier temperatures were similar to those expected in the reactor. The vessel liner, which was water-cooled, was maintained at 150° to 190°F.

Nitrogen gas was purged through five inlets in the top, which extended below the thermal barrier. This purge system duplicated the condition in the reactor in which nitrogen is accidentally introduced into the upper plenum through the control rod drive mechanisms. The purge rate for the experiment was maintained at 525 cm³/min, which is a direct volume scale of the maximum nitrogen ingress rate for the reactor.

Located at the top of the simulated access penetration was a series of traps (May packs) similar to those used in the King furnace tests previously described. A scintillation crystal positioned opposite the first trap (copper gauze) monitored the buildup of I¹³¹ activity outside the access penetration.

Iodine (tagged with I¹³¹) was vaporized and introduced into the apparatus through a quartz tube, which was sealed at the end but was drilled with six 1/16-in. holes over a 12-in. span. Thus, entry of iodine vapor into the apparatus from a diffuse source was similar to the effusion of iodine from the coolant holes in the top of the core.

Two runs were performed. In the first, 6 g of iodine was introduced between the fifteenth and fortieth hour of operation. After 60 hr of operation, the May pack was dismantled and analyzed. During the second run, an additional 25 g of iodine was purged into the system between the twentieth and thirtieth hour of operation. Thus, by the end of the second run, the total iodine introduced was 31 g, which is scaled to a direct surface area loading in the reactor upper plenum. The second run was 62 hr long.

Figure 3.35 shows the buildup of iodine in the May pack assembly during the second run. Table 3.21 gives the results of the radioassay of the access penetration and May pack assembly. It is seen that only 7.6×10^{-4} of the total iodine (or 23.5 mg) was found in the copper and charcoal traps; 2.5×10^{-5} fraction of the total iodine added (or 0.78 mg I) was located in the carbon trap. This fraction can be thought of as being due to the "penetrating form," perhaps as methyl iodide.

Fig. 3.33--Apparatus for 1/12-scale reactor plenum simulation test

Fig. 3.34--Temperature data from 1/12-scale reactor plenum simulation test (run No. 2)

Fig. 3.35--Release of I¹³¹ from simulated reactor plenum experiment

Table 3.21

DISTRIBUTION OF I^{131} IN REACTOR PLENUM SIMULATION APPARATUS^a

Location	Fraction	
	Run 1 6 g I_2	Run 2 31 g I_2
1. Vessel liner	---	~0.20
2. Thermal barrier	---	~0.70
3. Shield plug thermal barrier	---	9.3×10^{-2}
4. Shield plug	---	1.6×10^{-3}
5. Access penetration 0 to 4 in.	---	7.8×10^{-4}
6. Access penetration 4 to 16 in.	---	2.1×10^{-4}
7. Access penetration exit flange	---	6.4×10^{-5}
8. Copper gauze trap	5.2×10^{-4}	7.4×10^{-4}
9. Millipore filter	(b)	(b)
10. Carbon trap	2.9×10^{-5}	2.5×10^{-5}
11. Silica gel trap	2×10^{-6}	(b)

^a-Apparatus internals were examined only at the end of the second run.

^b-None detected.

Table 3.22 lists the results of the radioassay of the internal components of the apparatus, including the core barrel, the vessel liner, seven annular (side) thermal barriers, and eight circular (top) thermal barriers. These parts were scanned through a 4-in. by 4-in. collimator. The multiplicity of complicated parts precluded determination of a precise material balance. The analysis did indicate, however, the relative plateout of iodine on each component. It is seen that iodine plateout on hot iron surfaces occurs to a large extent at intermediate temperatures, 390° to 660°F. This phenomenon is consistent with the previously observed volatility behavior of FeI_2 .

The following conclusions are derived from the results of the King furnace and one-twelfth-scale core-heatup experiments:

1. Iodine reacts readily with hot iron surfaces to form iron iodides that condense (or plate out) essentially quantitatively at temperatures below about 570°F.
2. Condensation and plateout of iodine species on the thermal barriers and vessel liner of the reactor will result in very little iodine entering a failed access penetration; and plateout along the walls of the diffusion plug and access penetration will limit even further the escape of iodine out of the PCRVR. Thus, migration of iodine to a reactive or cold surface is much more important in removing iodine from the gas phase in the reactor vessel than is escape through an open penetration.
3. The maximum value of 5.5% release for iodine observed in previous King furnace experiments is very conservative.

Table 3.22

RADIOASSAY OF INTERNAL PARTS OF ONE-TWELFTH-SCALE REACTOR
PLENUM SIMULATION EXPERIMENT

Location	Peak Temp (°F)	Fraction of I^{131}
Vessel Liner:		
Top	190	0.063
Sides	190	0.131
Top Thermal Barriers:		
1	1420	0.0014
2	~1240	0.0032
3	~1070	0.0016
4	~900	0.0043
5	~750	0.12
6	~570	0.10
7	~390	0.051
8	~210	0.051
Side Thermal Barriers:		
1	1390	0.014
2	~1250	0.087
3	~930	0.095
4	~750	0.099
5	~570	0.172
6	~390	0.0095
7	~210	0.022
Core Barrel and Reflector Surface	1580	Negligible

Fuel Migration

Experiments were conducted to determine the rates of migration of uranium and thorium from PyC- and SiC-coated fuel particles contained in fuel rod specimens into a graphite crucible during heatups of around 3000°C. The experiments were also designed to determine the effect of long times at high temperature on the physical stability of fuel rod specimens.

The fuel rod specimens used for these experiments were approximately 1 in. long by 0.5 in. in diameter and contained fertile particles with ThC_2 kernels and fissile particles with UC_2 kernels. The particles were Spraygraph-coated before being incorporated in the matrix. The fuel rods were loaded into graphite crucibles made from either 9567 (Nuclear III) or H327 graphite. The wall thickness of the crucibles was 0.2 in., which corresponds approximately to the minimum web thickness between a fuel hole and a coolant hole in the fuel element. The samples were heated inductively using a graphite

susceptor tube with a 0.5-in. wall. The sample temperature was monitored with an optical pyrometer calibrated to 2300°C using a U.S. National Bureau of Standards calibrated tungsten filament lamp. Temperature calibrations above 2300°C were extrapolated from the calibration data.

Three experiments on PyC-coated particles were conducted, one for 4 hr, one for 8 hr, and one for 100 hr. All three experiments were carried out in an inert atmosphere at temperatures above 3000°C, as shown in Table 3.23. After annealing, the compacts were easily removed from the crucibles, and the over-all appearance of the crucibles and compacts was excellent, as shown in Fig. 3.36. No rapid flow of a liquid U-Th-C phase between the compacts and crucibles had occurred. No gross distortion of the crucibles was evident, and it appears from weight-loss determinations that very little graphite vaporized from the crucibles. The fuel particles retained their physical shape even though metallographic examination (see Fig. 3.37) and radiographic examination (see Fig. 3.38) showed appreciable migration of uranium and thorium through the PyC coating and into the matrix. Uranium and thorium appeared to be spread throughout the fuel rod specimens, while only a small transfer of the metals into the crucible walls could be detected by chemical analysis. Weight losses for the crucibles, the fuel rods, and the combined assemblies are given in Table 3.23. Concentration profiles of uranium and thorium in the graphite crucibles are currently being determined in an attempt to calculate diffusion coefficients.

Table 3.23
WEIGHT LOSSES OF FUEL RODS CONTAINING BISO PARTICLES
AFTER ANNEALING ABOVE 3000°C

Run No.	Time at Temp (hr)	Avg. Temp (°C)	Atmosphere	Crucible Graphite	Crucible Weight Change (%)	Fuel Rod Weight Change (%)	Crucible and Fuel Rod Weight Change (%)
1	4	3150	He	9567	---	-2.3	---
2	8	3200	Ar	H327	-0.43	-3.2	-1.1
3	100	3050	Ar	9567	-0.45	-14.3	-3.3

The fuel rod specimen containing the SiC-coated particles was annealed for 10 hr at 3090°C in an argon-atmosphere induction furnace. After the anneal, the fuel rod specimen was intact and was easily removed from the graphite crucible by gently tapping on the crucible bottom. As shown in Fig. 3.39, very little difference was noted between the annealed specimen and the sample originally prepared. The fuel rod specimen lost 14% by weight, part of which could be seen adhering to the bottom of the crucible. Although the particles retained their physical shape, radiographic analyses showed substantial migration of uranium and thorium through the SiC coatings, similar to that observed in PyC-coated particles. Metallographic examination of the compact showed gross loss of silicon out of the coatings; chemical analysis,

Fig. 3.36--Fuel compact with BISO particles: top, before heatup experiment; bottom, after heatup experiment

Fig. 3.37--Metallographic section of BISO-coated particles (left, before heatup experiments; right, after heatup experiments)

Fig. 3.38--Radiograph of BISO-coated particles (left, before heatup experiment; right, after heatup experiment)

Fig. 3.39--Fuel rod specimen with TRISO particles (top, before heatup experiment; bottom, after heatup experiment)

now being performed, will show to what extent. The graphite crucible appeared to be unchanged except for a slight weight increase, which is probably due to compact material that remained in the bottom of the crucible. The crucible and specimen lost a total of 3.2% by weight. The experimental results are essentially the same as those reported for PyC-coated fuel compacts, except for the possible migration of silicon into the crucible walls. Analyses are being conducted to determine the degree of migration of uranium, thorium, and silicon into the graphite crucible.

Sorption and Diffusion of Fission Products

Dynamic Sorption of Iodine on Metal Surfaces. Experiments to measure the sorption of iodine on mild steel at low temperatures have been performed. The experimental procedure is to pass iodine vapor tagged with I^{131} ($P_{I_2} = 5 \times 10^{-6}$ atm) in helium through four 3/4-in.-ID mild steel tubes maintained at 25°, 80°, 135°, and 200°C. A copper gauze plug at the end of each tube traps the iodine that penetrates the entire length. The tubes are frequently removed from the apparatus and scanned with a scintillation crystal to determine the axial iodine plateout profiles.

The results of the iodine sorption experiments after 550 hr of operation are given in Table 3.24.

Table 3.24
DEPOSITION OF IODINE ON MILD STEEL

Temp (°C)	Iodine Sorbed at Temp ($\mu\text{g}/\text{cm}^2$)	Total Iodine in Copper Gauze Trap (%)
25	30-100	36.6
80	12-30	76.6
135	10-15	70
200	8-13	79

Figures 3.40 through 3.43 show the distribution of iodine along each of the steel tubes. It is seen that a step increase in iodine sorption occurred at about the center of the tubes, particularly for the 25° and 80°C specimens. This phenomenon is believed to be caused by slightly different surface pretreatments at opposite ends of the tubes. Each tube was immersed half way into rust-removing solutions (Alconox and Turco solutions), and the ends were reversed frequently. This slightly different pretreatment is the only explanation that can be offered for the discontinuous nature of iodine deposition in an essentially isothermal zone. (Differences in surface character, such as oxide content, are expected to be minimized at higher temperatures.)

Neglecting the spurious effects of pretreatment, the observed low levels of sorption and the high percentage of iodine in the copper gauze trap downstream from each tube suggest that at these temperatures, the iodine plateout

Fig. 3.40--Iodine sorption on mild steel at 25°C

Fig. 3.41--Iodine sorption on mild steel at 80°C

Fig. 3.42--Iodine sorption on mild steel at 135°C

Fig. 3.43--Iodine sorption on mild steel at 200°C

mechanism is largely physical adsorption, with very little chemisorption and/or reaction to form FeI_2 . In contrast, previous tests, conducted at higher temperatures, showed that iodine is effectively removed from a flowing gas stream at 300° to 500°C by reaction with the hot metal surface to form FeI_2 (see previous quarterly report, GA-8270).

Strontium-Graphite Dust Sorption Experiments. A long-term strontium sorption experiment using the Sr Dust B method previously described (see GA-8038) is continuing. The purpose of this work is to determine the relative sorption of strontium on graphite dust and a mild steel surface under equilibrium conditions.

In this test, five Incoloy 800 tube specimens were coated with Sr^{85} -tagged graphite dust. The tubes are being maintained in a thermal gradient ranging from 300° to 800°C in a helium atmosphere. At the end of the test the specimens will be individually gamma scanned, before and after blowdown and wiping procedures, to determine the quantity of dust that can be removed by blowdown and the relative strontium sorption on the metal and graphite dust. This experiment is being run for a long period of time because it is believed that the sorption data obtained using this technique will be meaningful only after a long period of equilibration, especially at the lower temperatures. The test will be concluded and analyzed after about four months of operation.

HELIUM PURIFICATION SYSTEM

The objectives of this subtask are to:

1. Determine operating conditions of the plant's sorbent beds as a function of bed size and temperature, coadsorbate interference, regeneration techniques, aerosol removal, and adsorption bed materials, by means of laboratory-type experiments.
2. Predict the extent of carbon transport and deposition in the plant's primary coolant system by means of analysis and out-of-reactor laboratory-scale testing of the effects of temperature, materials, coolant gaseous impurities, and surface pretreatment.

Sorbent Beds

Hydrogen Gettering Studies. An additional dynamic hydrogen gettering test has been performed. The apparatus consisted of a packed bed of titanium sponge 0.58 in. ID by 10 in. long. Five taps, spaced 2 in. apart along the bed length, permitted measurement of the hydrogen concentration at various positions in the bed during the experiment. In this way, the "front" of hydrogen moving through the bed can be determined.

In this test the bed was maintained at 370°C (700°F) while a prepurified gas mixture composed of 500 ppm H_2 in helium was purged through the bed at 1.58 liters (STP)/min.

Figure 3.44 shows the hydrogen concentration at 2-in. increments in the bed versus the total volume of gas purged through the column. In Fig. 3.45,

Fig. 3.44--Dynamic sorption tests of hydrogen on titanium (H_2 concentration versus integrated gas flow)

Fig. 3.45--Dynamic sorption tests of hydrogen on titanium (H_2 concentration versus bed length)

the hydrogen concentration data are plotted versus bed length. As expected, the hydrogen front becomes less steep with time or total gas flow.

This test bed is presently being regenerated at 530°C by purging it with purified helium. After this treatment, another dynamic hydrogen sorption run will be performed.

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Task IV
FUEL ELEMENT PROOF TESTING

The objective of this task is to demonstrate suitable performance of a test fuel element(s) based on the plant fuel element design by means of irradiation testing at expected fuel operating temperatures at greater than expected temperature gradients and up to or exceeding approximately one third of the planned fuel element burnup in the plant.

The first proof test element was shipped to the Peach Bottom site the latter part of the previous quarter and will be inserted during the current plant shutdown. This element contains both BISO and TRISO type coated particles.

A second element is being fabricated which will be the same as the first element, except that it will contain all TRISO fuel particles. Fabrication of the second element is under way.

The fissile and fertile particles selected for the second proof test element are of the following types: TRISO-I LTI, TRISO-I HTI, TRISO-II LTI, and TRISO-II HTI.

The matrix material formulation selected for the fuel rods is 47.4 wt-% R-72S phenolic resin and 21 wt-% GP-38 filler with excess amounts of mixed anhydride curing agent. The rods, which are 1.00 in., 5.93 in., 11.53 in., and 14.31 in. long, will be fabricated by the simultaneous metering particle loading system and the injection-molding rod fabrication technique. Most of the rods will be carbonized by heating to 1000°C in a nitrogen purge, though a few will be heated to 1000°C in a vacuum. The rods will be carbonized in place in the test element graphite fuel sections.

All of the necessary fissile material has been manufactured and accepted. In addition, the LTI fertile particles, both TRISO-I and TRISO-II, have been fabricated. The LTI TRISO-I particles have been accepted, and the LTI TRISO-I fuel rods are currently being fabricated. The HTI-coated fertile particles are currently in process.

PART II

PRIVATELY SPONSORED RESEARCH AND DEVELOPMENT PROGRAM

(Not the Subject of AEC Reimbursement)

Task I
FUEL TRANSFER MACHINE

The objective of this task is to establish the suitability and maintainability of the fuel transfer machine for use in the plant by means of testing of the fuel transfer machine with a segment of a dummy core, and by carrying out tests in helium at the temperature and chemical impurity level expected in the plant of critical components, as necessary.

During the quarter all drawings for the fuel transfer machine have been released for fabrication. Orders have been placed for some of the components, and a large percentage of the raw material has been received. The large components, such as the upper housing assembly and the centrifugally cast mechanism guide tubes, are out for quotations. Fabrication of the grapple head components is about 90% complete. Assembly of the grapple head will begin when the bellows actuators and the ball spline have been received.

Analysis of the vertical Z-drive motion of the telescoping guide tubes has been done for various postulated failure modes, such as tubes binding in the guide rails causing seizure and loss of speed control when bringing the tubes to a stop.

As a result of the analysis, three monitoring systems are being investigated: an inner and outer tube separation switch, a tension load cell in each of the two wire rope cables that support the outer tubes, and utilization of the existing differential pressure transducers in the Z-drive speed synchronization circuits to monitor the torque output of the hydraulic motors. With these devices, an inner tube seized to the outer tube and/or an outer tube seized to the outer guide rails can be sensed and the appropriate machine motion can be actuated.

Results of the Z-drive analysis showed that it would be desirable to provide a snubber device between the contact surfaces of the inner and outer guide tubes when the inner tube picks up the outer tube in the upward direction. A similar device would be desirable at the upper limit of tube travel where the outer tube contacts a stop. These devices would not be required during normal operation, because the machine is programmed to reduce speed before it makes contact at these points. However, if it is postulated that the control function is lost for some reason during the high-velocity motion in the vertical direction, the high impact forces might cause damage.

To determine the forces involved and design parameters for the snubbing devices, two computer programs, SMASH and IMPACT, were developed to compute the loadings based on various velocities, spring rates, masses, etc. The proposed snubbers would utilize high-rate springs in the form of stacked

Belleville washers that provide a compact configuration that can be readily installed within the space available.

A failure mode analysis and a maintainability analysis were prepared. The failure mode study showed the various failure conditions of the transfer machine components and the consequences of their failure. Each component was rated as to whether it was a critical item and whether any backup machine corrective function exists. The maintainability study identified the probable repair or replacement action that would be needed for the various components. The approximate time needed to repair or replace the component and the extent that the hot service facility would be needed was calculated.

Work is continuing in the programming for the fuel transfer machine automatic control computer, effort being concentrated on flow block diagrams. A punched paper tape and printed page data output format is being studied.

The digital logic circuits have progressed through the breadboard stage, and prototype printed circuit cards are under construction. A multiplex analog-to-digital converter has been selected to convert analog signals to digital form for use by the computer.

Design of the circuits needed to complete the analog servo systems is continuing. A portion of the circuits is being breadboarded and environmental testing is scheduled.

The vertical hydraulic drive servo system is being studied. The parameters necessary to make a system stability analysis were determined, and these have been requested from the drive system supplier.

A failure mode and maintainability analysis was completed for the entire machine control system. The system was divided into components, and a failure mode and repair time was developed for each.

The general arrangement drawing for the fuel handling machine control room has been revised to incorporate changes in the console and cabinet locations and the cable trench locations. Specification drawings and purchase requisitions have been released for the operator's console and the three equipment cabinets. Designing of panels and other associated hardware is continuing.

The construction contract for the Gulf General Atomic test tower modifications has been let. Bids have been received for the finish-machined graphite dummy core components and for the iron castings that will be used to simulate the steel-clad upper layer reflector elements. Procurement of the dummy core components is under way.

Drawings for the core support structure are being reviewed and finalized for release for fabrication.

The autoclave for the grapple head test has been delivered complete with surfaces sprayed with aluminum metal and the heaters and insulation installed. The autoclave power wiring, temperature control, and test cycle control equipment is being installed. Work has started on the extension of the helium

purification system plumbing to incorporate this test facility.

Some preliminary planning has begun for a test of the torque motors and tachometer generators used in the grapple head positioning drives. The purpose of the test would be to evaluate the life and wear characteristics of the brush and commutator surfaces in helium at elevated temperature.

Task II

SERIES-STEAM-TURBINE-DRIVEN HELIUM CIRCULATOR

The objective of this task is to establish the suitability of the main helium circulators and steam turbine drives for use in the plant by means of analysis, component tests, and prototype testing under conditions of temperature and speed expected in the plant and up to at least 50% of design horsepower. Compressor blade and rotor vibration measurements will be made during these tests. The duration of the full-scale prototype testing will be approximately 200 hr.

PROTOTYPE DEVELOPMENT

A set of new high-stiffness journal bearings was designed for the new prototype on the basis of the observed thrust bearing slipper performance. As a consequence of increased pocket width and pocket depth, the over-all bearing length had to be increased to alleviate a sharp reduction in damping coefficients. It is calculated that the pocket modification combined with orifice compensation increases bearing stiffness by approximately a factor of 10.

A comparison of calculated spring rates (new configuration) with the existing bearing spring rates is shown on Fig. II.1. Comparison of estimated damping coefficients is shown on Fig. II.2. The estimate shows no change in turbine bearing damping, but a reduction of the compressor bearing damping by a factor of 2. Further evaluation of damping coefficients is being carried out in cooperation with Mechanical Technology, Inc. (MTI).

As a result of bearing changes there was an increase in shaft length of 2.0 in. with the bearing span remaining the same. Computer results of rotor amplitudes versus speed for the new set of bearings is shown on Fig. II.3 (using 3 in.-oz unbalance in the compressor wheel plane). Figure II.4 shows the computer-calculated rotor response with the existing bearings and a comparison with the test data points run with the same amount of unbalance.

On the basis of these results, the following conclusions can be drawn:

1. Maximum amplitude response points at compressor and turbine bearing locations are shifted out of the normal operating speed range (zero to 9550 rpm). In parallel with this, occurrence of pronounced conical mode is greatly alleviated.
2. Maximum amplitudes at speeds above 11,000 rpm are substantially increased especially in the range of new resonant points of 13,000 to 14,000 rpm. This is caused by the sharp drop in damping-to-spring-rate ratios.

Fig. II.1--Spring rates versus speed

Fig. II.2--Damping coefficient times angular velocity versus speed; bearing parameters shown on Fig. II.1

Fig. II.3--Rotor response with modified bearings, 3 in.-oz in the compressor wheel plane

Fig. II.4--Build 4, computer results versus test results; 3 in.-oz in compressor plane

3. Maximum unbalance capacity for the normal operating speed range (zero to 9550 rpm) has been increased from approximately 12 in.-oz for existing prototype bearings to approximately 25 in.-oz for the new bearings. Both values here were calculated under the assumption that the spring rates and the damping coefficients do not increase as the shaft approaches the bearing wall.
4. With new bearings, the maximum unbalance capacity at 14,000 rpm (45% overspeed) would be in the range of 3 to 4 in.-oz derived under the same assumptions as in 3.
5. An increase in the turbine journal water flow from 8 to 21.5 gpm and in the compressor journal water flow from 8 to approximately 43 gpm has been calculated.
6. The journal friction power would increase by approximately 5 to 6 hp at 9550 rpm prorated on the basis of the original MTI estimate.

The over-all performance of the helium compressor in air has been evaluated using the computer program described in the previous report. The results are presented in Fig. II.5, which shows a plot of compressor over-all static-to-static pressure rise ratio versus normalized air weight flow for the various normalized rotative speeds tested. The experimental data agree well with the predicted performance for the range of operation expected in the reactor. However, there is some deviation at the extreme low-flow condition for each speed due to compressor-blade stall. This blade stall is apparently caused by flow separation along the blade tip inlet contour which results in a decrease in performance greater than predicted. It should be noted that the predicted curves were based on a straight inlet with no boundary layer at the blade tips. For the 180° bend of the compressor inlet, the boundary layer has built up to the point where it affects blade performance at the low-flow condition for each speed.

Work is progressing on the reduction of compressor blade cascade data taken with the probe actuators during the air tests.

A specification outlining the initial prototype helium circulator testing with steam has been completed. From this, a detailed specification was written to delineate the exact testing procedure. These specifications describe the testing required to determine the aerodynamic performance of the turbine and compressor and the performance and operation of the bearings and seal systems. Also, from these specifications additional instrumentation (thermocouples on the vessel and circulator, thermocouples in the steam and helium flow stream, and pressure taps in the steam flow) was designated and has been installed in the circulator.

Prototype testing with steam commenced during the quarter, and the conditions summarized in Table II.1 have been tested. Preliminary data indicate that much less steam flow was required to obtain a desired rotative speed than was predicted. This was attributable to the poorly cast turbine nozzle installed in the assembly. The trailing edges of the blades were bowed inward, restricting the flow area by about 25% to 30%. This constriction gave a larger velocity at the throat for a given steam flow rate and therefore more available

Fig. II.5--Prototype compressor air tests

Table II.1
PROTOTYPE HELIUM CIRCULATOR STEAM TEST RESULTS

Condition No.	Date (1968)	Circulator Speed (rpm)	Turbine Inlet Pressure (psia)	Turbine Discharge Pressure (psia)	Turbine Pressure Ratio	Turbine Inlet Temp (°F)	Turbine Discharge Temp (°F)	Steam Flow (lb/sec)	Helium Temp (°F)	Helium Pressure (psia)
20	1/23	2000	160	154	1.04	575	568	6.7	52	245
21	1/23	4034	204	182	1.12	598	570	18.8	79	268
22	1/23	4013	206	177	1.16	597	567	20.1	84	257
23	1/23	6025	235	172	1.37	620	560	30.5	110	260
30	1/24	6002	227	166	1.37	614	558	29.6	97	266
31	1/24	6058	238	157	1.52	625	555	32.2	135	258
32	1/24	8035	282	148	1.91	644	533	42.7	112	307
39	1/25	5930	240	167	1.44	619	552	30.7	102	294
40	1/25	7790	325	193	1.68	652	555	51.5	101	361
41	1/25	7850	325	171	1.90	651	540	51.5	105	362
42	1/25	5990	350	279	1.25	651	600	51.4	89	362
43	1/25	5960	348	268	1.30	645	586	50.2	88	364
44	1/25	5970	286	197	1.45	658	590	37.2	89	360
45	1/25	6000	298	207	1.44	656	588	41.3	92	364
51	1/26	3850	352	326	1.08	632	611	38.2	75	371
52	1/26	3860	276	248	1.11	625	599	27.0	75	372
53	1/26	3860	304	272	1.12	633	606	31.1	75	379
54	1/26	3830	307	273	1.13	632	605	30.6	72	374
55	2/1	3880	328	300	1.09	642	619	35.3	78	379
56	2/1	3890	327	287	1.14	640	615	36.0	80	370
57	2/1	3850	322	286	1.13	639	613	37.5	83	373
58	2/1	3880	330	287	1.15	640	614	39.0	85	371
59	2/1	6800	292	188	1.55	640	560	38.6	115	380
60	2/2	6750	302	187	1.62	640	550	44.4	120	377
61	2/2	6750	302	175	1.73	640	550	44.4	127	377
62	2/2	6920	299	158	1.89	640	540	42.7	145	377
63	2/2	8250	325	164	1.98	649	530	51.1	145	373
64	2/2	7910	325	164	1.98	648	530	51.4	150	370
65	2/3	7840	341	164	2.08	653	535	53.9	415	377
66	2/3	8030	336	168	2.00	654	545	53.3	545	377

power. A new set of nozzles has been cast and will be installed as soon as final machining is completed.

PROTOTYPE CIRCULATOR MALFUNCTION

A circulator failure occurred on February 3 at 2:30 p.m. The circulator had operated for approximately 158 hr with helium pressures up to 360 psia at various temperatures up to 650°F and was being driven by steam. The steam turbine inlet conditions were very nearly constant at 340 psig and 640°F.

During this test period there were two shutdowns. The purposes of the first, from January 26 to January 31, were to repair helium leaks in the accessory system valves and to effect some revisions to the steam system controls. The second shutdown was necessitated by a helium leak in the accessory system when a pipe connection broke on February 2. The leak was repaired by welding, and testing resumed 2 hr later. The period of no rotation when the machine was allowed to sit on the thrust bearing was 1/2 hr. When the bearing water was turned on again, the rotor reached a self-turbining speed of 160 rpm. After operation was resumed, the machine was brought up to a speed of 8000 rpm and the helium temperature was allowed to increase by cutting back the cooling water flow to the heat exchanger in the test vessel. Since the maximum steam flow was limited by the PSC turbine downstream of the circulator and by the turbine nozzle in the circulator steam turbine driver, it was decided to achieve design helium temperature at approximately half the design helium pressure. The machine was to be shut down to change nozzles, at which time it was planned to open the top of the vessel, allowing both ends of the machine to be inspected. In the resumption of testing it was planned to change the downstream main steam turbine operation so that two turbines could be operated, thereby increasing the available steam flow.

The failure began with a noise which sounded at the test facility as if a piston engine airplane were passing overhead. Prior to the incidence of the noise, methods for achieving temperature equilibrium in the test vessel were being reviewed which necessitated a check of the water flow and valves near the vessel outside the control room. The control room operator was relaying temperature readings to the men at the flow measuring station. As the noise continued and increased in volume, the speed dropped from 9100 to 8250 rpm. At that time the control room operator energized the emergency trip and the machine decelerated very rapidly. The counter read 2500 rpm and then zero, indicating that the time for deceleration was slightly greater than 10 sec, the counting interval.

Most personnel, including the control room operator, timed the noise interval at 10 sec, although two people who were not wearing headsets measured a 30-sec period. Assuming that hearing was impaired by the headsets or the closed control room door, 30 sec can be taken as the interval of the noise. No observation was made of the oscilloscopes that displayed the displacement probe results during the noise period, and they were not recorded on tape. However, they had indicated previously that the lower journal bearing was operating at an eccentricity ratio of 0.25, which was constant, except for its speed dependency, for the entire test.

At the compressor end, rotor blades were severely damaged by Swagelok fittings that fell from the outside of the diffuser into the inlet and were reflected from the inlet struts and outer shroud and the inlet rotor blades. There was also a 1/16-in. deep by 1/16-in. wide groove cut into the compressor disk. At the turbine end of the machine outboard of the water seal, there was shaft bending and severe rubbing of the rotor parts on the stator parts. The section between the bearing housings showed moderate damage to the bearings and seals.

The possible causes of failure can be separated into two categories: (1) that the ingestion of the fittings resulted in the rubbing at the turbine end of the machine, and (2) that the damage at one end of the machine was dissociated from that at the other (assuming that the time interval between the compressor and turbine damage was greater than 30 sec) and that the fittings were shaken loose after the rubbing began.

The possible failure causes of the first category are:

1. A rigid body resonance was excited, possibly due to balance change, which caused bearing rub. This may have enlarged the lower journal bearing sufficiently to allow the labyrinth and/or thermal gradient insulation part to rub, thereby overheating the shaft and leading to bending and failure of the shaft at this point.
2. The impact of the ingested particles excited the free-free mode of the shaft (26,000 rpm) causing rubbing in the lower end of the machine.
3. The frequency of impact of the fittings between the rotating blades and the strut excited the free-free resonance.
4. A particle resulting from the ingestion lodged between the compressor disk and insulation cover and machined the groove in the disk. This operation exerted enough force to tilt the rotor so that the shaft rubbed on the thermal insulation piece resulting in outboard shaft bending.
5. The free-free resonance was excited by the particle lodging between the side and insulation cover when the machining began.

The following are possible causes for failure of the second category:

1. Rubbing began in the lower end of the machine (thermal insulation piece).
2. The upper journal bearing loosened in the housing.
3. Foreign material in the lower end caused rubbing.
4. The tie bolt was loosened by expansion.

It is estimated that the rebuilt prototype circulator will be ready for further test runs late in April 1968. A new prototype will be ready for test in about August 1968.

PRIMARY COOLANT SHUTOFF VALVE

The light-weight check-valve described in the previous quarterly report is being developed as an alternative to the present reference design for the primary coolant shutoff valves. Predicted performance curves are presented which illustrate that the response of the valve is satisfactory for use in the primary coolant system as proposed. A test valve has been fabricated and tested to verify this prediction. Bench test results indicate that there is an undesirable binding in the hinges; however, air flow tests show the valve response is better than predicted even for low frictional resistance. These tests are discussed below.

The primary coolant shutoff valves are mounted directly above the circulators in the reactor; the coolant flow is vertical as it passes through the valves. Each valve utilizes two semi-elliptical plates on a common support hinge. A valve is maintained in the open position (valve plates together) by the lift torque generated by a small airfoil mounted on each valve plate. The valve closes due to gravity. No actuator, spring-counterbalance, or weight-counterbalance system is provided. When closed, the valve plates are supported by radially inclined webs in order to withstand the pressure differential produced by the operating circulators with minimum plate thicknesses.

Figures II.6 and II.7 illustrate the predicted performance of the light-weight check-valve for various coefficients of friction. These figures were developed as follows:

1. The "aerodynamic moment" on the valve plate without airfoil was first determined for various angles from the expression

$$M = C_m P_{\text{dyn}} D^3 \quad ,$$

where M = moment, in.-lb

C_m = moment coefficient, dimensionless

P_{dyn} = dynamic pressure in annular flow area below valve,
lb/in.²

D = characteristic dimension of valve, in. (taken as diameter of cylinder at lower diffuser)

The value of C_m at various angles was determined during a previous test on the spring-counterbalanced check-valve as discussed in the previous quarterly report.

2. The moment due to the airfoil was determined by calculating the lift as a function of dynamic pressure and multiplying by the moment arm for the valve plates at the full-open position, where the angle of attack of the airfoil is in accordance with the theoretical lift coefficient used. The "lift moment" was assumed to decrease to zero at a valve plate angle of 15° (0° is full open) as a linear function of the plate angle for a given dynamic pressure.

Fig. II.6--Light-weight check-valve characteristics

Fig. II.7--Light-weight check-valve characteristics; see Fig. II.6 for legend

3. The sum of the above two linear functions of the dynamic pressure was then plotted for various angles as shown on Fig. II.6.
4. The static moment of the valve plate is determined from the expression

$$M_w = Wx \sin(\theta + \theta_0) \quad ,$$

where M_w = static moment of valve plate, in.-lb,

W = weight of valve plate, lb,

x = distance from hinge centerline to plate center of gravity, in.,

θ = plate angle from full open, degrees, and

θ_0 = correction for position of center of gravity at $\theta = 0^\circ$, degrees.

The value of M_w is simply a horizontal line on Fig. II.6 for a given angle. The intersection of this line with the line plotted as noted in 3 above yields the dynamic pressure at which the valve plate will be in equilibrium for the given angle. Plotting a series of the above points yields the valve equilibrium position curve as a function of flow. This curve has been translated to Fig. II.7 to eliminate the moment parameter.

5. The effect of friction was approximated by assuming a constant hinge load equal to the plate weight. The friction moment is then

$$M_f = frW \quad ,$$

where M_f = friction moment, in.-lb,

f = friction coefficient, dimensionless,

r = hinge radius, in.,

W = plate weight, lb.

Adding or subtracting the calculated value for various values of f from the static moment, M_w , allows plotting additional equilibrium position curves. Figures II.6 and II.7 show such curves for several values of f . The heavy arrows indicate whether the valve is opening (flow increasing) or closing (flow decreasing).

Possible sources of error in the method described above include the fact that the moment due to the airfoil as a function of valve position was assumed for simplicity to be a linear function of plate angle, and that the friction resistance was assumed to be independent of valve plate angle and/or flow.

A series of experiments was performed on a test valve employing this concept. Determination of the moment required to move each valve plate, $M_w \pm M_f$, has shown that the effective coefficient of friction is much higher

than expected. The problem is apparently due to binding caused by poor hinge alignment, rather than an actual high friction coefficient.

In order to determine the general valve characteristics and to identify any further problem areas, the air flow test was performed despite the binding problem. The valve position as a function of flow determined in this test is shown on Fig. II.7. It is seen that the valve performance is much better than predicted. The plates open and close in a narrow flow range which is below all but one expected operating point (one circulator operation at zero reactor power with normal helium inventory). The circulator flow corresponding to this condition is 2% of the design flow rate. Because of flow requirements for the core, the minimum flow may be increased to 3%, which would make the flow range in which the valve operates below all expected operating conditions. No oscillation of the valve plates occurred during the air flow test, which is a desirable situation.

An interesting characteristic of the valve performance was that one plate lagged the other in responding to changes in flow. This characteristic was observed to be in agreement with the difference in the effective coefficients of friction between the plates. This characteristic is not considered to be undesirable if it exists after the binding problem is solved.

Figure II.8 presents the experimentally determined combined moment coefficient, C_m^* , for the light-weight valve. The combined moment coefficient includes the previously described moment coefficient, C_m , for the valve plate and a term to reflect the moment created by the lift on the airfoil. Also indicated on Fig. II.8 are the values of C_m^* used for predicting the valve performance. The latter values were determined from experimental values of C_m from the previous valve test and from calculated values from the airfoil effect, which is zero above 15° . The higher experimental values of C_m are consistent with the fact that the valve performance was better than expected. This is illustrated in Fig. II.9, which shows an enlarged scale plot of the experimental results of valve position versus velocity head (see Fig. II.7) and the calculated position based on the experimental values of C_m^* .

Tests were also performed to determine the values of C_m for the light-weight plates without the airfoils. As noted on Fig. II.8, there appears to be no improvement in the moment coefficient due to the airfoils, that is, $C_m \approx C_m^*$. Since this was an unexpected result, a further test was performed to determine the position of the valve as a function of flow with the angle of attack of the airfoil increased by 6° . No improvement in performance was observed. As a result of these tests, the airfoils will be deleted from the design concept.

A design study for the actuated valve design has been completed by Stratos Western Division of Fairchild Hiller Corporation. The selected arrangement is a butterfly valve which utilizes a circular valve plate that is "buried" in the diffuser cone when the valve is open. The cone is sectioned so that portions of it rotate with the plate to the closed position. Simplified calculations have been made to size the actuator, and an assembly drawing of the valve has been prepared. Because of the difficulty in calculating the

Fig. II.8--Light-weight check-valve test, C_m versus N_R for plate No. 1

Fig. II.9--Light-weight check-valve test, free position versus velocity head

torque requirements for opening and closing a butterfly valve with this unusual geometry, a test to determine the torque requirements is under consideration. A wood and metal mockup has been fabricated.

Task III
CONTROL ROD DRIVE

The objective of the work under this task is to determine the suitability, maintainability, and reliability of the control rod drives for the plant under the expected temperature and helium chemical impurity conditions predicted for the reactor by means of testing, including accelerated life testing, of a full-scale prototype of the control rods and drive.

As stated in the previous quarterly report (GA-8420), the Phase II environmental test program was suspended because of the ingress of debris into a clutch shaft bearing.

Further problems associated with the winding of the brake and clutch coils prompted a decision to change from a 125 vdc to 24 vdc and gain the advantage of a substantial increase in magnet wire diameter and strength. Precise procedures were developed for the winding, vacuum impregnation, encapsulation, and general handling of such coils.

The brake assembly spider hub, which was made of a magnetic material, caused distortion or bypassing of the flux path and resulted in a decrease in the magnetic efficiency. The hub has been remade of high-tensile aluminum alloy with a marked improvement in tractive force.

In addition, a parallel flow and vibration test on the control rods indicated a need for design changes. In order that the control rod drive mechanism test program might be continued, it was decided to replace the rods with dummy weights. When the rods are modified, they will be replaced in the test facility.

The test program was restarted. The "single phasing test" repeatedly demonstrated that with one motor phase inoperative and the rods withdrawn, a command to reverse direction resulted in immediate rod insertion.

A simulated electrical malfunction test was performed on the capacitive scram system to determine the effect of various types of failure on system reliability. The only failure condition that resulted in a loss of retardation was found to be with the capacitors connected in delta and one leg shorted. As a result, the wye arrangement will be used.

The rod cycling test was resumed at the rate of nine 2-in. jogs per min, but this was judged to be too severe a duty and it was reduced to six jogs per minute. After 60,000 cycles, the scram characteristics exhibited erratic behavior. This became pronounced after 77,800 cycles and the test was discontinued, although the drive remained operative in the shim mode.

The problem was found to be in the fast-scam retarder unit. This unit is not part of the reference design but has been under study as a possible additional feature. The rotor had fouled in the stator to a point at which both it and the bearings had seized. No other part of the mechanism was affected, and the test was resumed with the retarder removed. The retarder failure is being studied to determine the cause of failure. Operation was normal for a further 40,200 jogs, when it was observed that the scam time had increased by 5%. Shortly afterward, the drive friction increased so that a scam cycle could not be performed, and the motor input had increased from 175 to 300 w. However, the drive continued to be operable in the shim and jogging modes.

On removal from the autoclave, it was found that the brake-end motor bearing had seized. The motor shaft had made about 9×10^6 revolutions at this time. Figure III.1 shows the condition of the bearing. It was concluded from examination of the bearing and analysis of local debris that an excessive amount of solid film had been removed during the burnishing process before bearing assembly. A replacement bearing was therefore assembled and engineering surveillance was imposed on the burnishing process.

There was no detectable change in the friction characteristics of the remainder of the control rod drive mechanism; the test was therefore resumed, and is continuing at the rate of approximately 10,000 jogs and 1,500 ft of rod travel per day. To date the following operations, which represent 15% of the specified cycling test, have been performed:

Jogs	240,900
Scrams	486
Total rod travel, ft	50,105
Time in environment, hr	856
Motor revolutions	16,735,070

Fig. III.1--Bearing components showing condition
(top, balls; center, retainer half; bottom, inner
race)

Task IV
STEAM GENERATOR

The objective of this task is to establish the suitability and maintainability of the design of the steam generators for the conditions of temperature and pressure expected in the plant by means of conceptual design, analyses, and testing of the critical aspects of the design. The tests are expected to include gas flow distribution and pressure drops, helium-side heat transfer, steam/water flow stability, tube vibration, and experimental investigation of material compatibility with coolant impurities. The development of a high-temperature design basis and investigation of experience relative to tube integrity is also planned.

TUBE RESTRAINT TESTING PROGRAM

Initially, this program of vibration testing in pure hot helium was intended (1) to investigate possible self-welding, fretting wear, or other metallurgical effects between steam generator tubes and their support plates, and (2) to determine the reduction in fatigue strength due to such interaction. After a number of test runs, the test program objective was modified to determine the metallurgical damage only. Steam generator tubes, support plates, and restraint were simulated, and the total environment was simulated by vibrating the tubes, heating, and providing a helium atmosphere.

Tube restraint performance is evaluated by simulating aerodynamic vibration of the tube in an environment comparable to the PSC steam generator and in the configuration proposed for the PSC design. Tube and plate materials tested were Sanicro 31 tubes with Incoloy 800 plates and ferritic tubes SA-213-T22 with the comparable plates SA-387. The test configuration shown in Fig. IV.1 consists of two test vessels with supporting equipment that can operate independently. The performance of each environmental system is separately monitored. The natural frequency of the tube, restraint, and plate configuration is established by "tuning" the shaker for maximum amplitude, and the tube is mechanically vibrated at this frequency. Test amplitude and duration are established to develop a relative standard of specimen performance.

Combinations of joint variables were evaluated to find a joint configuration that would produce the minimum amount of fretting damage at the joint restraint.

At the completion of each test run the specimens are removed from the vessel. The tubes are then removed from the plate and restraint by hand where possible. Preliminary observations are made on the condition of the plate, sleeve, and tube, and the specimens are prepared for detailed metallographic analysis.

Fig. IV.1--Steam generator tube restraint test

Heat is supplied from strip heaters which are fastened against the tube sheet. The ferritic plate is heated to 1000° and the Incoloy 800 plate to 1400°F. Plate temperature is monitored by a temperature recorder for each vessel. A determination of the inner tube temperature is made prior to vibrating by inserting a thermocouple the length of the tube until it is opposite the tube restraint. A temperature reduction of approximately 80° from plate temperature was noted on each configuration, resulting in a ferritic tube temperature of approximately 920° and a Sanicro 31 tube temperature of 1320°F.

Helium purity levels are monitored with a gas chromatograph. The test environment simulated the reactor primary system atmosphere of less than 10 ppm of oxygen and less than 50 ppm of CO and CO₂. To maintain a reducing atmosphere in the vessel, helium with 500 ppm of hydrogen was used in all tests. Moisture monitor readings were also taken.

To achieve the appropriate environment the vessel is evacuated and then back-flushed with helium. The vessel is heated, pumped down, back-flushed, and heated again. This purging is done as required until the appropriate helium purity levels and temperature are achieved.

Tube deflection amplitude is measured through a window in the vessel opposite the shaker. The deflection is measured at the end of the tube 12 in. from the restraint by a K & E sight level with a micrometer head. Tube deflections can be read within ± 0.002 in.

Table IV.1 lists the material and configuration for each test. Summarizing from the table, the number of tests for each of the 25 different materials combinations listed in the order of increasing test number were Incoloy 800 (1), nickel (1), chrome plate on carbon steel (1), chrome plate on 410 stainless steel (1), Tufftride on carbon steel (1), Gramix 278 (1), chrome carbide on 410 stainless steel (1), Gramix 548 (1), nickel aluminide flame spray on 410 stainless steel (1), copper (1), cast iron (1), TD nickel (1), wire mesh stainless steel (1), 4340 steel (1), Stellite flame spray (1), columbium (1), Stellite hardened (2), annealed 310 stainless steel (2), silver plate on carbon steel (2), copper plate on 410 stainless steel (2), alumina flame spray (3), for 2 Incoloy and 2 ferritic specimens no restraint was inserted but the plate was crimped or reduced (4), Inconel 718 (5), and Stellite 6B (13).

A total of eight different restraint designs were tested. The first restraint design tested was the tapered wedge as shown in Fig. IV.2. The next design tested was a rolled sleeve tapped into place between the plate and the tube with a tab against the plate (see Fig. IV.3). The third restraint tested, the double-collar sleeve (see Fig. IV.4), was more frequently tested than any other restraint. The collar extended beyond the width of the plate on each side and kept the sleeve restraint within the plate. The fourth type was similar to the double-collar sleeve, except that the inside diameter had a narrow raised portion to provide a reduced area of surface contact on the tube (see Fig. IV.5). The fifth sleeve was a full-ring sleeve that was 1/2-in. wide and press fitted into the plate (see Fig. IV.6). The next sleeve design, which was used on only two tests, had a hardened sleeve welded into the plate (see Fig. IV.7). The wire mesh restraint design was only used for

Table IV.1

STEAM GENERATOR TUBE RESTRAINT TESTS

No.	Vessel	Date	Tube	Plate	Restraint Material	Restraint Design	Fastening Method	Test Duration	Amplitude (in.)	Freq. (cps)	Inner Mass (lb)	Center Mass (lb)	Outer Mass (lb)	Comments on Tube Condition
1	I	8/27/67	Sanicro 31	Incoloy 800	Incoloy 800	Tapered Wedge	Plate "C" punched	6 min	0.3	22	9.5	none	9.5	Severe fretting
2	I	9/7/67	Sanicro 31	Incoloy 800	none	none	Single crimp	130 min	0.6	24	9.5	none	9.5	Tube fractured, severe fretting and self welding
3	I	9/20/67	Sanicro 31	Incoloy 800	none	none	Single crimp	6 min	0.3	19	9.5	37.9	9.5	Moderate self welding
4	I	9/27/67	Ferritic SA-213-T-22	Ferritic SA-387	Annealed 304 SS	Rolled sleeve (split)	Tab welded on plate	2 hr	0.44	19	9.5	37.9	9.5	Moderate damage
5	I	10/2/67	Ferritic SA-213-T-22	Ferritic SA-387	Annealed 304 SS	Rolled sleeve (split)	Tab welded on plate	8 hr	0.44	21	9.5	37.9	9.5	Large areas of severe fretting
6	I	10/6/67	Ferritic SA-213-T-22	Ferritic SA-387	Nickle	Rolled sleeve (split)	Tab welded on plate	4 hr	0.44	21.3	9.5	37.9	9.5	Small areas of fretting
7	II	10/15/67	Ferritic SA-213-T-22	Ferritic SA-387	none	none	Single crimp	4 hr	0.44	23	9.5	37.9	9.5	Tube fractured, large areas of severe fretting
8	I	10/14/67	Ferritic SA-213-T-22	Ferritic SA-387	Chrome plate 0.0005 on C.S.	Double collar	Triple crimp	4 hr	0.44	20	9.5	37.9	9.5	Small areas of moderate fretting
9	I	10/16/67	Ferritic SA-213-T-22	Ferritic SA-387	Silver plate 0.001 on C.S.	Double collar	Triple crimp	4 hr	0.08	19	9.5	37.9	9.5	Slight fretting, Ag plate rubbed off
10	II	10/16/67	Ferritic SA-213-T-22	Ferritic SA-387	Inconel 718	Double collar	Triple crimp	4 hr	0.08	21	9.5	37.9	9.5	Very slight damage
11	I	10/17/67	Ferritic SA-213-T-22	Ferritic SA-387	Copper plate 0.004 on C.S.	Double collar	Triple crimp	4 hr	0.08	20	9.5	37.9	9.5	Copper burnish on tube surface
12	II	10/17/67	Ferritic SA-213-T-22	Ferritic SA-387	Chrome plate 0.0005 on C.S.	Double collar	Triple crimp	4 hr	0.08	21	9.5	37.9	9.5	Small areas of fretting
13	I	10/18/67	Ferritic SA-213-T-22	Ferritic SA-387	Al oxide flame spray 0.006 on 410 SS	Double collar	Triple crimp	4 hr	0.08	20	9.5	37.9	9.5	Slight to moderate damage
14	II	10/18/67	Ferritic SA-213-T-22	Ferritic SA-387	Tufftride surface 0.003 on C.S.	Double collar	Triple crimp	4 hr	0.08	25	9.5	37.9	9.5	Slight to moderate damage
15	I	10/19/67	Ferritic SA-213-T-22	Ferritic SA-387	Inconel 718	Double collar Red area	Triple crimp	4 hr	0.08	19.5	9.5	37.9	9.5	Burnishing
16	II	10/19/67	Ferritic SA-213-T-22	Ferritic SA-387	Gramix 278 sintered bronze	Double collar	Triple crimp	4 hr	0.08	26	9.5	37.9	9.5	Very slight damage

Table IV.1 (continued)

No.	Vessel	Date	Tube	Plate	Restraint Material	Restraint Design	Fastening Method	Test Duration	Amplitude (in.)	Freq. (cps)	Inner Mass (lb)	Center Mass (lb)	Outer Mass (lb)	Comments on Tube Condition
17	I	10/19/67	Ferritic SA-213-T-22	Ferritic SA-387	Chrome carbide flame spray 0.006 on 410 SS	Double collar	Triple crimp	4 hr	0.08	19	9.5	37.9	9.5	Moderate damage
18	II	10/20/67	Ferritic SA-213-T-22	Ferritic SA-387	Gramix 548 sintered bronze	Double collar	Triple crimp	4 hr	0.08	26	9.5	37.9	9.5	Severe fretting, metal transfer, and burnishing
19	II	10/20/67	Ferritic SA-213-T-22	Ferritic SA-387	Inconel 718	Double collar	Triple crimp	4 hr	0.41	25	9.5	37.9	none	Severe fretting damage
20	II	10/21/67	Ferritic SA-213-T-22	Ferritic SA-387	Copper plate 0.004 on C.S.	Double collar	Triple crimp	4 hr	0.41	22.5	9.5	37.9	none	Slight fretting damage, and copper transfer to tube
21	I	10/20/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite 6B	Sleeve (solid)	Triple crimp	4 hr	0.08	22.5	9.5	37.9	9.5	Very slight damage
22	I	10/21/67	Ferritic SA-213-T-22	Ferritic SA-387	Ni Aluminide flame spray 0.006 on 410 SS	Double collar	Triple crimp	4 hr	0.08	25	9.5	37.9	9.5	Moderate damage
23	II	10/23/67	Ferritic SA-213-T-22	Ferritic SA-387	Inconel 718	Single collar, Red area	Tab bent upward	4 hr	0.41	26.5	9.5	37.9	none	Two areas of severe fretting damage
24	I	10/24/67	Ferritic SA-213-T-22	Ferritic SA-387	Inconel 718	Double collar	Triple crimp	10 ⁶ cycle	0.08	38.5	3	10	0	Slight fretting damage
25	II	10/24/67	Ferritic SA-213-T-22	Ferritic SA-387	Al Oxide flame spray 0.006 on 410 SS	Double collar	Triple crimp	4 hr	0.41	26	9.5	37.9	0	Several areas of severe fretting damage
26	I	10/24/67	Ferritic SA-213-T-22	Ferritic SA-387	Copper	Double collar	Triple crimp	4 hr	0.08	29.5	3	10	0	Very slight damage, some copper transferred to tube
27	II	10/25/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite 6B	Sleeve (solid)	Triple crimp	4 hr	0.41	26	9.5	37.9	0	Slight fretting damage
28	I	10/25/67	Ferritic SA-213-T-22	Ferritic SA-387	Cast iron	Double collar	Triple crimp	4 hr	0.08	36	3	10	0	Slight damage
29	II	10/26/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite hardened	Sleeve (welded to plate)	Triple crimp	4 hr	0.41	26	9.5	37.9	0	Fretting damage and metal transfer
30	I	10/26/67	Ferritic SA-213-T-22	Ferritic SA-387	Al Oxide 0.006 flame spray on 410 SS	Double collar	Triple crimp	10 ⁶ cycle	0.08	33	3	10	0	Fretting damage

Table IV.1 (continued)

No.	Vessel	Date	Tube	Plate	Restraint Material	Restraint Design	Fastening Method	Test Duration	Amplitude (in.)	Freq. (cps)	Inner Mass (lb)	Center Mass (lb)	Outer Mass (lb)	Comments on Tube Condition
31	I	10/27/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite 6B	Sleeve (solid)	Plate "C" punched	10 cycle	0.08	35	3	10	0	Slight fretting
32	II	10/27/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite 6B	Sleeve (solid)	Plate "C" punched	4 hr	0.08	33	3	10	0	Moderate fretting damage
33	II	10/27/67	Ferritic SA-213-T-22	Ferritic SA-387	none	Contoured hole	Slip fit	4 hr	0.41	26	9.5	37.9	0	Fretting damage around full circumference of plate contact
34	II	10/28/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite 6B	Sleeve (solid)	Plate "C" punched	4 hr	0.41	28	9.5	37.9	0	Severe fretting damage
35	II	10/30/67	Ferritic SA-213-T-22	Ferritic SA-387	T D Nickle	Sleeve (solid)	Plate "C" punched	4 hr	0.08	27	9.5	37.9	0	Burnishing and slight fretting damage
36	I	10/30/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite 6B	Sleeve (solid)	Pressed into plate	10 ⁶ cycle	0.08	27	3	10	0	Severe fretting damage
37	I	10/31/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite 6B	Sleeve (split)	Backing ring	10 ⁶ cycle	0.08	40	3	10	0	Very slight damage and burnishing
38	I	11/1/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite hardened	Tapered sleeve (welded to plate)	Triple crimp	10 ⁶ cycle	0.08	29	3	10	0	Severe fretting damage
39	II	11/2/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite 6B	Sleeve (solid)	Pressed into plate	10 ⁶ cycle	0.08	26	9.5	37.9	0	Very slight fretting damage
40	I	11/2/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite 6B	Sleeve (split)	Backing ring	10 ⁶ cycle	0.41	40	3	10	0	Severe fretting damage
41	I	11/3/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite 6B	Tapered sleeve	Plate "C" punched	10 ⁶ cycle	0.08	39	3	10	0	Slight fretting damage
42	II	11/4/67	Sanicro 31	Incoloy 800	Stellite 6B	Sleeve (split)	Backing ring	10 ⁶ cycle	0.08	40	9.5	37.9	0	Severe fretting damage
43			Sanicro 31	Incoloy 800	(Specimen set aside)									
44	II	11/6/67	Ferritic SA-213-T-22	Ferritic SA-387	Silver plate 0.001 on C.S.	Single collar	Plate "C" punched	10 ⁶ cycle	0.08	27	9.5	37.9	0	Slight fretting damage
45	I	11/5/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite 6B	Tapered sleeve	Plate "C" punched	9.3 x 10 ⁶ cycle	0.016	34	3	10	0	No visible damage
46	II	11/7/67	Sanicro 31	Incoloy 800	321 S.S.	Wire mesh	Force fitted	4 hr	0.41	25	9.5	37.9	0	Burnishing at contact points, no visible damage
47	II	11/8/67	Ferritic SA-213-T-22	Ferritic SA-387	4340 C.S.	Tapered sleeve	Plate "C" punched	10 ⁶ cycle	0.08	29	9.5	37.9	0	Slight fretting damage
48	I	11/8/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite 6B	Sleeve (solid)	Plate "C" punched	27.2 x 10 ⁶ cycle	0.016	34	3	10	3	Burnishing, no visible damage
49	II		Ferritic SA-213-T-22	Ferritic SA-387	(removed from vessel prior to testing)									

Table IV.1 (continued)

No.	Vessel	Date	Tube	Plate	Restraint Material	Restraint Design	Fastening Method	Test Duration	Amplitude (in.)	Freq. (cps)	Inner Mass (lb)	Center Mass (lb)	Outer Mass (lb)	Comments on Tube Condition
50	II	11/10/67	Ferritic SA-213-T-22	Ferritic SA-387	Stellite flame spray 0.003 on 410	Tapered sleeve	Plate "C" punched	10 ⁶ cycle	0.08	29	9.5	37.9	0	Slight burnishing
51	II	11/11/67	Ferritic SA-213-T-22	Ferritic SA-387	Columbium	Tapered sleeve	Plate "C" punched	10 ⁶ cycle	0.08	27	9.5	37.9	0	Moderate fretting damage
52	II	11/17/67	Ferritic SA-213-T-22	Ferritic SA-387	Incoloy 800 outer, SA-336-F-22 inner	Double sleeves with splits	Long weld along split	10 ⁶ cyc	0.08	28	3	10	0	Small areas of metal transfer from tube to sleeve
53	I	11/17/67	Ferritic SA-213-T-22	Ferritic SA-387	Incoloy 800	Single sleeve with split	Two circum. welds	10 ⁶ cyc	0.08	50	3	10	0	Severe fretting and self welding on small areas
54	II	11/22/67	Ferritic SA-213-T-22	Ferritic SA-387	Incoloy 800	Single sleeve with split	Long weld along split	10 ⁶ cyc	0.08	26.5	3	10	0	Sleeve seamweld extended through to tube
55	I	11/27/67	Ferritic SA-213-T-22	Ferritic SA-387	17-4 P.H. outer, SA-336-F-22 inner	Double sleeves with splits	Two circum. welds	10 ⁶ cyc	0.08	27	3	10	0	Slight fretting damage
56	II	11/27/67	Sanicro 31	Incoloy 800	Incoloy 800 outer & inner	Double sleeves with splits	Two circum. welds	10 ⁶ cyc	0.08	42.5	9.5	37.9	0	Self welding between tube, sleeve, and plate
57	I	11/28/67	Ferritic SA-213-T-22	Ferritic SA-387	Incoloy 800	Tapered sleeve and hole	Sleeve welded to tube	10 ⁶ cyc	0.08	67	3	10	0	No visible tube damage under sleeve
58	II	11/29/67	Ferritic SA-213-T-22	Ferritic SA-387	Greek Ascoloy outer, SA-336-F-22 inner	Double sleeves with splits	Two circum. welds	10 ⁶ cyc	0.08	39	9.5	37.9	0	Sleeve tackweld extended through to tube
59	I	11/30/67	Ferritic SA-213-T-22	Ferritic SA-387	17-4 P.H. sleeve, SA-336-F-22 wedges	Tapered sleeve, two wedges	Inner wedges spotwelded to sleeve	10 ⁶ cyc	0.08	44.5	3	10	0	No visible damage
60	I	12/1/67	Ferritic SA-213-T-22	Ferritic SA-387	17-4 P.H. sleeve, SA-336-F-22 wedges	2-1/2° angled hole tapered sleeve, two wedges	Inner wedges spotwelded to sleeve	10 ⁶ cyc	0.08	41	3	10	0	No visible damage
61	I	12/2/67	Ferritic SA-213-T-22	Ferritic SA-387	410 sleeve, SA-336-F-22 wedges	2-1/2° angled hole tapered sleeve, two wedges	Inner wedges spotwelded to sleeve	10 ⁶ cyc	0.08	35	3	10	0	Tube surface was unmarked. Plate surface had self welding and fretting
62	I	12/6/67	Ferritic SA-213-T-22	Ferritic SA-387	Hasteloy B sleeve, SA-336-F-22 wedges	2-1/2° angled hole tapered sleeve, two wedges	Inner wedges spotwelded to sleeve	10 ⁶ cyc	0.08	46	3	10	0	Tube surface was slightly burnished. Plate surface was unmarked

Table IV.1 (continued)

No.	Vessel	Date	Tube	Plate	Restraint Material	Restraint Design	Fastening Method	Test Duration	Amplitude (in.)	Freq. (cps)	Inner Mass (lb)	Center Mass (lb)	Outer Mass (lb)	Comments on Tube Condition
63	II	12/6/67	Sanicro 31	Incoloy 800	Inconel 718 sleeve, Incoloy 800 wedges	2-1/2° angled hole tapered sleeve, two wedges	Inner wedges spotwelded to sleeve	10 ⁶ cyc	0.08	57	3	10	0	Tube burnished. Plate surface was unmarked
64	I	12/8/67	Ferritic SA-213-T-22	Ferritic SA-387	430 sleeve, SA-336-F-22 wedges	2-1/2° angled hole tapered sleeve, two wedges	Inner wedges spotwelded to sleeve	10 ⁶ cyc	0.08	46	3	10	0	Tube and plate were unmarked
65	II	12/12/67	Sanicro 31	Incoloy 800	Inconel 625 sleeve, Incoloy 800 wedges	2-1/2° angled hole tapered sleeve, two wedges	Inner wedges spotwelded to sleeve	1.29 x 10 ⁶ cyc	0.08	37	3	10	0	Tube burnished, plate unmarked
66	II	12/13/67	Ferritic SA-213-T-22 curved	Ferritic SA-387	410 sleeve, 2-1/4 Cr. 1 Mo. wedges	2-1/2° angled hole tapered sleeve, two wedges	Inner wedges spotwelded to sleeve	10 ⁶ cyc	0.08	45	3	10	0	Tube burnished, self welding and fretting on sleeve and plate
67	II	12/14/67	Ferritic SA-213-T-22 curved	Ferritic SA-387	410 sleeve, 2-1/4 Cr. 1 Mo. wedges	2-1/2° angled hole tapered sleeve, two wedges	Inner wedges spotwelded to sleeve	10 ⁶ cyc	0.08	45	3	10	0	Tube damaged by tackweld on sleeve, sleeve self welded to plate
68	II	12/15/67	Ferritic SA-213-T-22 curved	Ferritic SA-387	430 sleeve, 2-1/4 Cr. 1 Mo. wedges	2-1/2° angled hole tapered sleeve, two wedges	Inner wedges spotwelded to sleeve	10 ⁶ cyc	0.08	39	3	10	0	No damage noted on tube, sleeve or plate
69	I	12/16/67	Ferritic SA-213-T-22 curved	Ferritic SA-387	Inconel 600 sleeve	Single sleeve with split	Circum. weld to tube	10 ⁶ cyc	0.08	38	3	10	0	Tube unmarked, slight fretting between sleeve and plate
70	II	12/16/67	Ferritic SA-213-T-22 curved	Ferritic SA-387	Incoloy 600 sleeve	Single sleeve with split	Circum. weld to tube	10 ⁶ cyc	0.08	28	9.5	37.9	0	Slight damage between sleeve and tube. Moderate self welding of sleeve to plate
71	I	12/19/67	Ferritic SA-213-T-22 curved	Ferritic SA-387	410 sleeve 4130 wedge	Tapered sleeve, split wedge	Inner wedges spotwelded to sleeves	10 ⁶ cyc	0.08	45	3	10	0	Sleeve was tight on plate. No visible damage
72	II	12/19/67	Ferritic SA-213-T-22 curved	Ferritic SA-387	Hastaloy B sleeve 4130 wedge	Tapered sleeve, split wedge	Inner wedges spotwelded to sleeves	10 ⁶ cyc	0.08	41	9.5	37.9	0	No visible damage to tube or plate
73	I	12/20/67	Ferritic SA-213-T-22 curved	Ferritic SA-387	Hastaloy B sleeve 4130 wedge	Tapered sleeve, split wedge	Inner wedges spotwelded to sleeves	10 ⁶ cyc	0.08	26	3	10	0	Damaged T/C caused excessive plate temp. (approx. 1400°F)

Table IV.1 (continued)

No.	Vessel	Date	Tube	Plate	Restraint Material	Restraint Design	Fastening Method	Test Duration	Amplitude (in.)	Freq. (cps)	Inner Mass (lb)	Center Mass (lb)	Water Mass (lb)	Comments on Race Condition
74	I	12/21/67	Ferritic SA-213-T-22 curved	Ferritic SA-387	Hastaloy B sleeve 4130 wedge	Tapered sleeve, split wedge	Inner wedges spotwelded to sleeves	10 ⁶ cyc	0.025	37	3	10	3	Specimen was removed and sleeve inspected after 5 x 10 ⁷ cyc. It was then tested up to 10 ⁸ cycles. No significant wear was noted on the outer sleeve
75	II	12/27/67	Ferritic SA-213-T-22 curved	Ferritic SA-387	Hastaloy B sleeve 4130 wedge	Tapered sleeve, split wedge	Inner wedges spotwelded to sleeves	10 ⁶ cyc	0.08	47	9	37.9	0	No sleeve damage but edge of wedge rubbed on tube
76			Ferritic SA-213-T-22 curved	Ferritic SA-387	Hastaloy B sleeve 4130 wedge	Collared tapered sleeve, split wedge	Inner wedges spotwelded to sleeves	SPECIMEN WAS NOT TESTED						
77			Ferritic SA-213-T-22 curved	Ferritic SA-387	Hastaloy B sleeve 4130 wedge	Collared tapered sleeve, split wedge	Inner wedges spotwelded to sleeves	SPECIMEN WAS NOT TESTED						
78	II	12/29/67	Sanicro 31 curved	Incoloy 800	Inconel 718 sleeve Inconel 800 wedge	Collared tapered sleeve, split wedge	Inner wedges spotwelded to sleeves	10 ⁶ cyc	0.08	53	3	10	0	Moderate self welding and fretting between sleeve and plate. No damage observed to tube wedge and I.D. of sleeve
79			Sanicro 31 curved	Incoloy 800	Inconel 718 sleeve Inconel 800 wedge	Collared tapered sleeve, split wedge	Inner wedges spotwelded to sleeves	SPECIMEN WAS NOT TESTED						
80	II	1/4/68	Sanicro 31 curved	Incoloy 800	Inconel 625 sleeve, Incoloy 800 wedge	Tapered sleeve, split wedge	Inner wedges spotwelded to sleeves	10 ⁶ cyc	0.08	43	3	10	0	Tube unmarked but one area 1/4" x 1/8" of metal transfer was observed on the sleeve
81	II	1/9/68	Sanicro 31 curved	Incoloy 800	Inconel 625 sleeve Incoloy 800 wedge	Tapered sleeve, split wedge	Inner wedges spotwelded to sleeves	10 ⁶ cyc	0.08	35	3	10	0	Tube undamaged. Small areas on sleeve show moderate fretting
82	II	1/14/68	Sanicro 31 curved	Incoloy 800	Inconel 750 sleeve Incoloy 800 wedge	Collared tapered sleeve, split wedge	Inner wedge spotwelded to sleeve	10 ⁶ cyc	0.08	46	3	10	0	No damage between sleeve and plate. Some metal transfer between tube and wedge
83			Sanicro 31 curved	Incoloy 800	Inconel 750 sleeve Incoloy 800 wedge	Collared tapered sleeve, split wedge	Inner wedge spotwelded to sleeve	SPECIMEN WAS NOT TESTED						
84	II	1/16/68	Sanicro 31 curved	Incoloy 800	Inconel 750 sleeve Incoloy 800 wedge	Collared tapered sleeve, split wedge	Inner wedge spotwelded to sleeve	10 ⁶ cyc	0.08	41	3	10	0	No damage between tube and wedge. Area of metal transfer 3/8" x 3/8" between sleeve and plate
85	II	1/19/68	Sanicro 31 curved	Incoloy 800	Inconel 750 H.T. sleeve Incoloy 800 wedge	Collared tapered sleeve, split wedge	Inner wedge spotwelded to sleeve	10 ⁶ cyc	0.08	47	3	10	0	Minor fretting between sleeve and plate. No damage between tube and wedge
86	II	1/25/68	Sanicro 31 curved	Incoloy 800	Inconel 750 H.T. sleeve Incoloy 800 wedge	Collared tapered sleeve, split wedge	Inner wedge spotwelded to sleeve	10 ⁶ cyc	0.08	58	3	10	0	Surfaces between tube and wedge had one small damaged area. Sleeve to plate surfaces were undamaged

Fig. IV.2--Tapered wedge

Fig. IV.3--Rolled sleeve

Fig. IV.4--Double collar

Fig. IV.5--Double collar with raised portion

Fig. IV.6--Solid sleeve

Fig. IV.7--Welded sleeve

one test. This design (see Fig. IV.8) completely filled the 0.080 in. difference in diameters around the tube. The eighth design, which was used in most of the recent tests, has a tapered sleeve inserted into a tapered hole (see Fig. IV.9).

Several other variations that were included with some of the types mentioned above included solid sleeve or split sleeve, surface finish on the sleeve, tube, and plate, clearance of ring or sleeve in plate or around tube, amount of chamfer (if any) on the sleeve, tab welded to or bent up against plate, and single or double collar.

Eight different methods were used to attach the restraint to the plate and tube. The first method used was to center punch the plate behind a tapered wedge driven between the plate and the tube. In the second method a single crimp was applied to each side of the plate. This technique supports the tube on the top and bottom of the tube (defining the top as the direction of gas flow, and bottom as 180 degrees around the tube). Unlike the full support that is offered across the top of the tube by the wedge, the crimp extends the outer surfaces of the plate farther toward the tube than the center of plate. This could cause stress concentrations at the two points of contact on the tube. The failure of two tubes using this restraint technique supports this observation. The third technique used was to insert a sleeve with tabs extending from the sleeve. The tabs were then welded to the plate. The fourth method, which was used for almost half of the tests on a variety of sleeve designs, includes a triple, or three-way crimp. The technique is similar to the single crimp, except that the crimps are narrower, there are three of them (the two additional crimps are located 45 degrees to either side of the top crimp), and the crimps are not as deep. The fifth method, which has recently been the most used restraining method, consisted of center punching a tapered hole after a tapered sleeve had been inserted into the hole.

With the sixth method no sleeve is used, and the hole in the plate for the tube is curved so that the inside diameter of the plate hole is only a few thousandths larger than the tube. Only one of these specimens was tested.

The seventh fastening method consists of a wire mesh (similar to a woven steel wool) stainless steel sleeve, force fitted by hand between the tube and the plate.

The eighth fastening method was to slip a backing ring behind the sleeve and install it in the plate by pressing it into the plate or welding it into the plate. The backing ring keeps the sleeve confined within the plate limits.

Four different large amplitudes (0.6, 0.44, 0.41, and 0.30 in. peak to peak) and two small amplitudes (0.08 and 0.016 in.) have been assigned to the test specimens. Each of these amplitudes has been assigned at different periods in the program.

The first three tests in the program used Incoloy 800 tube and plate material tested at 1400°F to evaluate fretting damage. The largest amplitude of 0.60 in. was used on only one test, and this was the second test of the

Fig. IV.8--Wire mesh

Fig. IV.9--Tapered hole and sleeve

program. The test was intended as a 24-hr evaluation of fretting damage to the tube, but the tube failed after the specimen had been running 2 hr and 10 min.

The 0.30 in. amplitude for a 6-min run was used in the first and third tests. Considerable evidence of fretting damage was noted in these three tests.

The initial five ferritic tests were performed at an amplitude of 0.44 in. The amplitude in this series (generally using a 4-hr test run) was discontinued when the second tube failure occurred. Ferritic tube test amplitudes were then decreased to 0.08 in., and testing time was increased to run 10^6 cycles.

Most of the tests since that time have been at an amplitude of 0.08 in. for 10^6 cycles. For specimens that have subsequently looked acceptable at this amplitude, a shorter (4-hr) test run has been scheduled at 0.41 in.

Recently two longer test periods at the smallest test amplitudes used to date (0.016 in.) have been run. These tests were run for 10×10^6 cycles and 27.2×10^6 cycles, respectively. Both of the longer duration tests used Stellite 6B.

The five configurations of tube counterbalancing weights and their sequence of use in the tests have been as follows:

1. Equal weights on each end of the tube (9.5 - 0 - 9.5 lb).
2. Equal weights on each end of the tube with a larger mass at the tube midpoint (9.5 - 37.9 - 9.5 lb).
3. A weight on the inner end of the tube and a larger weight at the midpoint (0 - 37.9 - 9.5 lb).
4. A smaller set of weights at the inner end of the tube and at the midpoint of the tube (0 - 10 - 3 lb).
5. The smaller set of weights with equal weights on each end of the tube and a heavier weight at the midpoint (3 - 10 - 3 lb).

The first two tests performed were in the configuration of equal weights on each end of the tube. The inner tube weight simulated the length of tube from restraint to a point midway to the adjacent baffle plate. The outer weight was added to balance the system.

The second weight configuration was established after a tube failure occurred in the second test. The system was considered unbalanced, the weights on each end moving in the same direction with no counteracting weight moving in the opposite direction. The second tube failure occurred with this configuration.

The third tube weight configuration reflected the fact that the forces generated against the tube are a function of the mass of the shaker armature. This is apparently observed by Southwest Research Institute test practices, but it is not universally followed in the field of vibration testing.

The fourth and fifth configuration of weights again reflect the fact that the shaker armature weight must be included in any consideration of the counterbalancing mass to be assigned to the tubes.

The change in the objectives of the test as noted above evolved from the lack of success in utilizing progressive damping traces to evaluate fretting damage and the increased testing required to evaluate proposed joint configurations. It had been planned to determine fatigue strength by exciting the specimen for 10^6 cycles, or until failure occurred, and then to examine the specimen. If no cracks were apparent, the displacement would be increased by 10% and run for another period of 10^6 cycles, or until failure occurred. It was noted in the early tests that when significant metallurgical damage occurred, it occurred within a few minutes of the start of the test.

Tube failure occurred at amplitudes selected to accelerate fretting damage considerably in advance of anticipated fatigue cycling periods. Consequently, metallurgical damage information was pursued, and fatigue strength evaluation goals were abandoned. Hence, all testing has been at amplitudes and time durations assigned to evaluate fretting.

One of the most significant variables consistently identified with tube and sleeve damage is tube deflection. At higher deflection values virtually all specimens appeared unacceptable, with damage varying from fretting on the tube and sleeve up to tube failure. At the two lower amplitudes some of the test runs had no visual damage from self-welding or fretting.

Beyond the apparent effect of changes in amplitude, the selection and range of variables in the test has been so wide that other trends in the data were difficult to recognize. The testing technique applied has been to try to cover as wide an area as possible to find a restraint design in the shortest time that could be built relatively inexpensively, installed easily, and last for 30 years.

All tests from No. 52 to date represent new tube restraint concepts. In this concept, a sacrificial sleeve is attached tightly to the tube so that any clearance in the tube restraint joint exists between the sacrificial sleeve and the plate, and not between the tube and the sleeve. This concept is designed to limit any wear to the sleeve rather than to the tube. An example of this design is shown by Fig. IV.10.

After an intensive evaluation and review of the test results, one long-term tube restraint test was performed on the ferritic sleeve design. The test joint was viewed at 50×10^6 cycles and no surface damage was noted. The test run was terminated at 10^8 cycles. The test amplitude gave joint action which was two times as severe as preliminary tube vibration testing results and calculations indicate. A preliminary study of the joint area indicated no damage to the tube or sleeve. The same design using high-alloy materials Inconel 718, Incoloy 625, and Inconel 750 has been studied. Incoloy 625 and Inconel 750 gave excellent tube protection in the screening tests run at 1400°F for 10^6 cycles at an amplitude approximately ten times greater than the vibration test results indicate. A long-term test using Inconel 750 is being prepared.

Fig. IV.10--Combination wedge-sleeve design

TUBE JOINT DAMPING TEST PROGRAM

In order to correctly evaluate the vibratory action of the steam generator tube bundles, the system damping characteristics must be known. Included in the parameters are material damping and joint damping. The first of these is being studied by Southwest Research Institute. The latter is to be determined by the test program.

The program will include experimental studies of the dynamic behavior of a straight length of tubing supported at the ends through simulated plate joints. The tube will be instrumented so that frequency and tube deflection (or strain) can be monitored. A means of simulating joint preload will also be incorporated into the test design.

In the test rig, a 4-ft length of tubing is supported by simulated tube-plate joints. The support structures are securely anchored to a rigid base which provides allowances for axial adjustment. The tube is excited by a horizontally mounted shaker which has a flexible link in the driver assembly. This link provides a decoupling of the shaker head mass from the tube. A load cell is also installed in the drive system which can be continually monitored during the test. At each support point is a joint preload mechanism, a calibrated spring which can be stretched to provide the desired joint preload.

Instrumentation consists of a set of four strain gages mounted at mid-span and set at quarter span of the tube. These gages are oriented at 90° increments around the tube and are connected in bridge circuits to provide measurement of in-plane (of input) and out-of-plane motion of the tube. Output from the bridges is recorded on a Visicorder.

Damping is determined from the band width of the system, which is the frequency difference between the half-power points. The equation for the magnification factor for a damped system is:

$$Q = \frac{f_n}{f_2 - f_1} ,$$

where Q = magnification factor, f_n = damped natural frequency, f_2 = upper half-power point, and f_1 = lower half-power point.

The test rig is being designed, and construction will begin immediately upon completion of the final drawing review. The load cells and other support equipment have been built and constructed for the test.

GAVEL VIBRATION TEST

The GAVEL air flow vibration test program at Sulzer is continuing, and all Visicorder traces of the tube vibrations have been read. A trial run of the computer program which analyzes the data indicated that some modifications were necessary to the program, and the computer program is being debugged. Bundle discharge velocity profiles were integrated over the flow

annulus, and the ratio of the measured mean velocity to the anemometer reading averaged 0.86. Previous velocity (anemometer) readings from GAVEL will be adjusted before computing flow excitation parameters. Pressure drop data from GAVEL were adjusted for flow distribution and then compared to the calculated values. The test pressure drop is 11% less than the calculated value. The testing that remains to be done using the GAVEL test facility includes further pressure-drop tests and the vortex shedding tests.

FLOW MALDISTRIBUTION

A study was completed on performance predictions of main steam tube circuits with the goal of reducing flow maldistribution arising from coil curvature and manifold effects. Since uniform orificing is to be installed, the performance deviations must be absorbed by a design metal temperature allowance.

The first phase of the study considered the 5% length reduction of coils in the innermost cylinder with the evaporator and SHII sections connected in series at the same cylinder location. This resulted in a predicted maximum temperature deviation in the mean wall of +31°F in the second inner cylinder. Coil circuit performance deviations were then developed using the installed mixed coupling between the evaporator and SHII cylinders and also incorporating the most favorable subheader manifold connection. This phase resulted in a predicted maximum deviation in the mean wall temperature of +20°F. The final phase incorporated subheader valve adjustment to eliminate hot subheader mixed deviations. This resulted in a final maximum deviation in mean wall temperature of +10°F.

STEAM-WATER FLOW STABILITY TEST PROGRAM

HEAT Computer Program

The HEAT computer code was developed as a tool for the steady-state performance analysis of heat exchangers of specified geometry. Emphasis was placed on the determination of temperature distributions as well as more standard performance information. HEAT, however, is somewhat limited in the kinds of geometries and unit configurations which it can analyze. Therefore, in order to analyze steam generators of the type being proposed for large HTGR plants, a new version of HEAT had to be written. This new version, called SUPERHEAT, provides analytical capability which has never before been available and which is very useful in predicting the performance of HTGR steam generators.

As HEAT is programmed, a heat exchanger is divided for computation purposes into autonomous units (called sections) which are interconnected only at their inlet and outlet points. In order to permit detailed analysis of HTGR steam generators, which consist of banks of helically coiled tubes, the SUPERHEAT code is being written (based on HEAT) to allow mixing of the helium between parallel tube banks. This task involved considerable reprogramming, since the computation method in HEAT follows the helium completely through

each section with no internal mixing with other sections. SUPERHEAT contains provisions for helium blending (in the circumferential direction along the tubes) or mixing (from one section to another in the radial direction) after each pass or after each tube in a bank between as many as five parallel sections (corresponding to concentric rings of helically coiled tubes).

Also, SUPERHEAT contains many other new features not found in HEAT. A new type of section has been devised to model the economizer-evaporator-superheater portion of the Fort St. Vrain steam generator. In this new "two-way" section, tube identification is maintained when transferring from a counter-current economizer-evaporator to a co-current superheater part way through the calculation.

The iteration scheme in SUPERHEAT has been programmed so that the computation will converge to a specified feedwater inlet enthalpy and, if desired, to a specified steam outlet pressure and a specified steam pressure drop. The input procedure is basically similar to HEAT's, except that additional options are available, and input for complicated unit configurations is much easier and more compact.

Boiling Stability Test Loop

Recent debugging of the loop has involved correction of leaks in the throttling and relief valves and the repair and recalibration of controls and instrument readouts. Performance runs involving the test section indicated that the finned coils were operating at approximately one-half their design thermal rating due to inferior performance of the internal baffling. The jackets and shrouds were disassembled and modifications made to eliminate bypass along the shroud walls and to increase gas velocity over the fin surfaces. Subsequent hot tests indicated that the fin heat transfer effectiveness in the test section now exceeds the design requirement. Hot testing also disclosed a gas flow deficiency through the test section of approximately 30% due to bypass-leakage paths. These paths were searched out with the aid of cold flow pressurization. Also, the test loop vendor has procured a larger-capacity air blower to give additional flexibility to loop operating conditions.

A diaphragm has been installed in a gas interspace between the test section and combustor. The diaphragm prevents any gas leakage from the combustor or ducting into the interspaces in bypassing the test section. Any leakage of hot gas must now escape from the outermost enclosure in order to bypass the test section. Pressurization during cold-flow leak testing indicates that the outer enclosure is completely sealed. Hot tests were initiated, but they were interrupted by flame-out of the combustor. Readjustment of the propane regulator and primary-secondary air settings were required. Pilot light ignition failed due to a short in the spark plug.

The maximum operating conditions reached by the system during a run to power were:

Feedwater temperature	400°F
Feedwater flow	955 lb/hr
Steam temperature	950°F
Combustion gas inlet	1100°F
Combustion gas exhaust	550°F
Loop pressure	2300 psia
Test section Δp	145 psi

At this point the steam control valve developed a leak at the seal between the valve cylinder and the body. The leak resulted in a rapid depressurization of the system to a point below the required NPSH of the circulating pump, and a complete shutdown was necessitated. The valve was repaired, and testing was resumed at the close of this report period.

STEAM GENERATOR TUBE-PLUGGING MACHINE

The construction drawings for the stationary housing and turret positioning system have been prepared. This equipment is used during both tube-plugging and reheat pipe seal welding operations. Alternate sets of special-purpose tools are placed on the turret for these operations.

The turret elevation and azimuth positions are controlled by an operator at the remote console. The turret may be rotated through a total travel of 400° at a maximum speed of 0.4 rpm with a 24-vdc stepping motor. Each step of the motor provides 0.01° of rotation or approximately 0.001 in. of movement at the circumference of the header. The total vertical travel of the turret is approximately 22 in. at a maximum speed of 7.2 in./min. A 24-vdc stepping motor powers this movement with each step providing approximately 0.0005 in. of movement.

The vertical and azimuth positions are transmitted to the control console with independent synchro systems. The resolution of the digital position indicators is 0.001 in. for both azimuth and vertical travel. A position-sensing tool in the turret is used to accurately determine the location of individual port openings during tube plugging. The over-all design criterion for the position sensor and the turret positioning systems is the capacity to orient the centers of the various tools within 0.030 in. of the actual center of the selected tube opening.

Another position sensor in the seal weld tool package is extended to contact an annular register surface just below the seal weld. The design criterion for the position sensor and the turret positioning system during seal welding operations is the capacity to orient the welding head and the weld cutter within 0.015 in. of the optimum position.

The stationary housing and turret positioning system also contains guide rollers, centering plungers, and limit switches to assure that the tube-plugging machine is properly aligned in the reheat pipe of the steam generator during repair operations.

All service lines (pneumatic, electric, cooling water, etc.) for the tube-plugging machine are routed through a sealed bulkhead at the lower end of the stationary housing. These lines are then routed either to equipment in the stationary housing or to the turret where they are connected to the various tools. Special clamps and arrangements are provided to assure that the service lines are not damaged during the combined rotation and elevation of the turret.

The construction layout for the reheat pipe seal weld tools is completed, and preparation of the construction drawings has been initiated. The seal-weld tools include:

1. Three centering rollers which are extended to contact a register diameter just below the seal weld.
2. An axial position sensor which contacts a horizontal register surface machined parallel with the weld joint, thereby providing precise axial positioning of the welding electrode and the cutting wheel.
3. A tungsten-electrode inert-gas-shielded welding head which is extended radially with a pneumatic cylinder. A positioning roller on a vertical axis contacts the weld joint to maintain the desired radial gap between the electrode and weld joint.
4. A weld cutter assembly which consists of a pneumatically powered cutting wheel mounted on a vertical axis and a linear actuator assembly to extend the cutting wheel into contact with the seal weld membrane.
5. A viewing system adapter consisting of lights and a mirror to allow viewing of the horizontal weld joint with the vertically mounted TV camera in the turret. The mirror may be rotated about a vertical axis with a dc motor to permit observation of either the welding operation, the cutting operation, or a close-up view of the completed weld.

Installation of the machine for seal welding is similar to the procedure used for tube plugging. When the machine is at the approximate working position for seal welding, the operator uses the viewing system and the elevation control system to orient the machine about 1/4 in. below the working position. The position sensor is then extended horizontally to a position just below the horizontal register surface. The entire turret assembly is then lifted until the position sensor indicates that the electrode is at the proper elevation.

The centering rollers are activated and the azimuth drive system for the turret is used to move the position sensor around the horizontal register surface of the weld joint to determine the optimum axial position for welding or cutting.

During the welding operation, the azimuth drive system moves the electrode tip in a circular pattern concentric with the horizontal weld joint. The entire welding operation is controlled by the automatic sequence timer at the control console. The estimated welding speed is approximately 6 in./min and approximately 7 min are required to make the complete weld. Cooling water flows through the weld head during this interval.

The lower portion of the reheat pipe has two small service lines running from the seal weld cavity to an access point outside the PCR. These tubes are used to introduce an inert shield gas to the back side of the joint during welding so that a mass spectrometer leak test can be performed, and they permit a pressure test of the weld. The quality of the finished weld is also evaluated by a visual examination with the TV system.

If the weld membrane must be cut for any reason, a 3-1/2-in. diam circular saw on a vertical axis is used. The membrane is approximately 0.100 in. thick and it will be cut through in one revolution around the pipe with the azimuth drive system. The cutting speed is expected to be between 60 and 90 surface ft/min and the expected feed rate is between 3 and 6 in./min.

After the weld joint is cut, the lower half of the weld joint must be rebuilt by manually adding weld metal while the lower segment of the reheat pipe is outside the steam generator. The cut is made just below the original weld joint to minimize subsequent repair of the upper portion of the seal weld membrane.

A preliminary outline of the test program for the tube plugging machine was prepared and submitted for project review during this period. The outline included a list of the tests which might be performed, a written description of the test chamber, and a tentative schedule for the test activity.

A conceptual design of the tube-plugging machine test stand as an integral part of the service equipment cart has been made and a drawing prepared.

The test stand consists of a series of cylindrical tube sections with inside diameters corresponding to the reheat headers and seal weld joint. Replaceable tube plugs and replaceable seal weld specimens are provided in the test structure.

An elevated platform surrounding the test tubes will allow easy access to observe tool operations and perform other test activity.

The construction drawings for the tube plugging machine support columns, handling equipment, etc., are being revised to correspond to recent changes in the steam generator outlet flange and internal register surface. Support equipment on the equipment cart is also being relocated to provide a more functional location for the equipment and to accommodate the integral test chamber described above.

Task V
COATED PARTICLE DEVELOPMENT

The objective of this task is to determine the process variables for fabrication of coated fuel particles suitable for use in the plant by means of investigation of various methods of fabricating fuel particles, investigation of coating process variables, and preirradiation evaluation of coated particles.

Development work on this program is directed toward effecting further economic and structural improvements in the fuel for the HTGR reactor system. This work includes the fabrication of coatings, the investigation of coating process variables, and the preirradiation evaluation of coated particles.

Until recently, the reference fuel particle was a BISO-coated particle that consisted of a buffer layer with an isotropic PyC overcoat. The coating of the reference fuel particle has been modified to include a thin layer of silicon carbide, and this new design has been designated the TRISO-coated particle.

BUFFER COATINGS

The density of the buffer layer of coated fuel particles is an important parameter. Buffer layers that are too dense do not provide a large enough volume for the fission-product gases; layers that are too porous do not provide enough carbon mass to attenuate the fission recoil products.

The density of a single particle cannot be determined, but techniques are available to calculate the average buffer density of a small sample, say 10,000 particles. A comparison was made of the two techniques in use at Gulf General Atomic. Three samples were used, and the difference in density was 0.05 g/cm³ or less in all cases. The agreement is satisfactory. The methods are described below, and the comparison data are shown in Table V.1. Both methods utilize the relationship that the density of the buffer layer is equal to the weight of the buffer layer divided by the volume of the buffer layer. In one method, the weight of the buffer layer is calculated from a chemical analysis of the constituents. The buffer density is calculated in the following manner:

$$\left(\frac{w}{x}\right) = A \frac{\text{g U/g particle}}{\text{g U/g core}} = \frac{\text{g core}}{\text{g particle}} \quad , \quad (1)$$

$$1 - A = B \frac{\text{g coating}}{\text{g particle}} \quad , \quad (2)$$

$$\left(\frac{1}{y}\right) = C \frac{\text{cm}^3 \text{ particle}}{\text{g particle}}, \quad (3)$$

$$\left(\frac{A}{z}\right) = D \frac{\text{g core/g particle}}{\text{g core/cm}^3 \text{ core}} = \frac{\text{cm}^3 \text{ core}}{\text{g particle}}, \quad (4)$$

$$C - D = E \frac{\text{cm}^3 \text{ particle}}{\text{g particle}}, \text{ and} \quad (5)$$

$$\left(\frac{B}{E}\right) = F \frac{\text{g coating}}{\text{cm}^3 \text{ coating}} = \text{buffer coating density}, \quad (6)$$

where w = weight percent metal in the buffer coated particle,

x = weight percent metal in the carbide core,

y = density of the buffer coated particle, and

z = density of the carbide core.

In the second method, the buffer coated particle is ignited in air to 900°C, and the weight of the buffer layer is calculated from the weight change; the buffer density is determined in the following manner:

$$\text{Buffer coating density} = \frac{(cd)(be - ag)}{(bce - dag)}, \quad \frac{\text{g}}{\text{cm}^3},$$

where a = weight percent metal in metal oxide,

b = weight percent metal in carbide core,

c = density of the carbide core,

d = density of the coated particle,

e = sample weight before ignition, and

g = ash weight after ignition at 900°C.

Table V.1
COMPARISON OF TWO METHODS OF DENSITY DETERMINATION

Batch No.	Density of Buffer Layer (g/cm ³)		
	Method 1	Method 2	Difference
HTDK-4675	1.04	1.09	0.05
HTDK-4677	0.97	1.02	0.05
HTDK-4678	0.92	0.96	0.04

Table V.2
THERMAL STABILITY TESTS ON METAL CARBIDE COATED PARTICLES

Batch No.	Temp (°C)	Time (hr)	Conclusions
3332-105	2300	4	Survived well
3332-127			Very poor, survived for 25 hr at 1850°C
3332-105	1850	25	Survived well
3332-127			Survived well
3332-47			Survived well
3332-49			Survived well
3332-51			Very poor, no PyC overcoat
3332-47	1700	200	Survived well
3332-49			Survived well
3332-51			Very poor, no PyC overcoat
3325-87	1400	18,000	No migration
3325-89			13-μ migration
3325-117			no migration
3332-139			8-μ migration
3332-141			No migration
3332-149			No migration
2107-31		8900	1-μ migration
1985-77			16-μ migration
3325-87			No migration
3325-89			35-μ migration
3325-117			No migration
3332-139			12-μ migration
3332-141			no migration
3332-149			No migration

COATING EVALUATION

Thermal Stability

A long-term stability test of BISO-coated particles has reached 5900 hr. These particles are at 1400°C under 2 psig helium. The next scheduled examination is at 9000 hr. This test contains one sample of HTI BISO-coated particles that had previously been tested for 9300 hr in vacuum at 1400°C, so that these particles have now accumulated 15,200 hr of exposure.

Two batches of SiC-coated particles have been tested at 1400°C in vacuo. Both batches contain a UC₂ core with successive layers of laminar carbon, SiC and laminar carbon. The examination after 8765 hr showed that all particles were intact. The test was terminated at 18,000 hr as planned, and the examination will soon be performed.

A second test consisting of four samples with an SiC layer and two samples with a ZrC layer has reached 8900 hr. This test is at 1400°C under 2 psig helium. Radiographic examination after 5000 hr indicated a heavy metal concentration just inside the buffer-ZrC interface in sample 3325-89E. It cannot be determined whether this is uranium migration outward from the core, or Zr migration inward. There was no change in any of the other samples. The next examination is scheduled after 10,000 hr.

A summary of all of the thermal stability tests that have been conducted on the HTGR program is given in Table V.2. All particle batches have silicon carbide as the metal carbide coating layer, with the exceptions of batches 3325-87 and 3325-89, which have zirconium carbide as the barrier coating.

Coated Particle Samples for Irradiation Capsules

A total of nineteen lots of BISO-coated particles prepared in large-scale (production) equipment were evaluated for irradiation testing in capsule

P19, and eleven lots were selected for the capsule. A list of samples is given in Task III, Part I of this report. The evaluation included visual, metallographic, and radiographic inspections of the coated particles and coating microstructural property (i.e., anisotropy factor) and geometry determinations. Several different density-anisotropy factor combinations will be tested for both HTI- and LTI-BISO coatings. One sample was separated into fractions with spherical cores and nonspherical cores, and both will be tested in the capsule to determine the effect of the irregular geometry of the latter on irradiation stability.

The planned irradiation testing program for TRISO-coated particles includes several P-capsule experiments. Samples for the first two capsules, P20 and P21, are being fabricated at the present time.

Data Collection System

A data collection and retrieval system that has been in use for a year has been modified and extended to include the coated particles made in production operations. Processing and experimental data for any particular batch of fuel particles is stored in a computer library tape and is available for easily readable recall. The comparison of coating properties with processing parameters is facilitated by this method of information storage. An example of the comparison that can be made is shown in Table V.2.

IMPROVED TECHNIQUES FOR ESTIMATING ANISOTROPY IN PyC

The irradiation performance of the coated HTGR fuel is influenced by the degree of preferred crystallite orientation within the nominally isotropic PyC coating. The present technique for measuring preferred orientation is based on a method described by Bacon (Ref. 1) and modified by Bokros (Ref. 2). This method uses X-ray diffraction techniques to determine the Bacon anisotropy factor (BAF) as a measure of isotropy in the PyC. The time required to make a precise BAF measurement for one sample is about 4 hr, BAF determination by the present technique would therefore represent a significant cost in quality control of HTGR fuel. The purpose of this work is to explore and develop alternative, less time-consuming methods of determining isotropy in PyC. The approach taken in this work was assumed that the desired technique would not necessarily have to determine a unique isotropy value for each sample. A method that could indicate the probable upper limit of BAF in a given PyC sample (for instance the PSC reference BAF upper limit, 1.10) would be useful for quality control purposes.

Optical Technique Using the Maltese Cross

When viewed between crossed polarizers of a metallograph, the cross section of an anisotropic PyC coating on a spherical particle displays a pattern similar to a "Maltese cross." This optical feature is produced by successive maxima and minima of light intensities at 90° intervals around the spherical particle. In previous work an attempt was made to correlate BAF and the intensity of the Maltese cross (see GA-8270). That work was extended during this reporting period.

In order to establish experimental limits for the BAF at the onset of Maltese cross formation, it was necessary to first determine the uncertainty of the BAF measurement using material from production batches and at least two operators. From two to four disk samples were taken from each of 19 batches of BISO particles prepared for capsule P19. One operator made one BAF determination for each batch, and the other operator made from one to three determinations for each batch. Individual BAF determinations for the batches ranged from 1.04 to 1.18. Assuming a normal distribution, the standard deviation of BAF measurement by X-ray was about 0.02. Therefore, there was about a two-thirds probability that the value of a single BAF determination would fall within ± 0.02 of the average value, which would be determined from a large number of measurements by different operators. The limits for a 95% probability of including the true average would be ± 0.04 .

The Cluster Mount. A technique was developed for preparing a metallographic mount of a cluster of several coated particles having different BAFs. The identity of the particles was preserved, and they could be polished and viewed under identical conditions. The technique could be used to compare coatings of unknown BAF with coatings of known BAF and possibly to determine the relative anisotropy.

After several experimental variations, the following optimum mounting and polishing procedure was adopted for all the work reported here:

1. Tape with adhesive on both sides was pressed onto a glass plate.
2. Particles (360 to 600 μ) from selected batches were placed on the tape in a prearranged order to maintain identity.
3. The plate and particles were warmed on a hot plate to about 50°C and a drop of catalyzed EPON 828 resin was placed on the particles.
4. The resin flowed around the particles and hardened. Then a 1-in.-ID plastic cylinder was placed over the cluster of particles and filled with catalyzed EPON 828 resin to complete the mount.
5. After the resin had cured the mount was removed from the plate and the following polishing technique was used for PyC-coated UC₂:
  - a. Grind on 320-grit SiC paper in kerosene to remove tape and penetrate to particles.
  - b. Grind on 600-grit SiC paper in kerosene to midplane of particles.
  - c. Polish with 6- μ diamond paste on cloth wheel in kerosene until 600-grit scratches are removed (about 1 min).
  - d. Repeat step (c) with 3- μ diamond paste.
  - e. Repeat step (c) with 1- μ diamond paste.
  - f. Final polish on vibrating platen (Buehler Vibromet) with 0.3- μ Al₂O₃ powder in water (about 30 min).
  - g. Wash in ethyl alcohol and air dry.
6. For PyC-coated cores containing ThC₂ the same polishing procedure was used with the exception that it was necessary to eliminate the final polish on the Vibromet because of rapid hydrolysis of the ThC₂.

After being washed with alcohol, the ThC_2 in the cores was passivated by immersion in a 50/50 HNO_2 - H_2O solution at room temperature for about 10 min. The treatment in HNO_3 sufficiently retarded the hydrolysis reaction that photographs could be taken before significant reaction took place.

The use of the Vibromet for final polish significantly increased the intensity of the Maltese cross. Therefore, particles having ThC_2 in their cores were not viewed under conditions as ideal as those having UC_2 cores. Attempts were made to circumvent this problem by polishing chordial sections of coatings on ThC_2 - UC_2 cores so that the core was not exposed. The Vibromet could be used in this situation for the final polish. However, the Maltese cross intensity for this condition was not significantly greater than that of an equatorial cross section. The fact that with a chordial section the lighting was oblique to the deposition plane rather than parallel to it may have contributed to lack of response to Vibromet polishing.

Low-temperature Isotropic Coatings (LTI). LTI-coated $(\text{Th,U})\text{C}_2$ particles (coated at 1400°C from propane) were selected from 10 separate batches. Nine of the batches had BAF values ranging from 1.00 to 1.03 and one had a BAF of 1.21. The particles were mounted in clusters with HTI coatings covering a similar range of BAF. There was no evidence of either high or low BAF LTI coatings showing a Maltese cross under polarized light, even though HTI coatings in the same mount having a BAF as low as 1.08 showed weak cross formation. Similar observations have been reported previously (GA-8270). Therefore, with LTI coatings this technique does not appear useful for anisotropy estimates in the BAF range of 1.00 to 1.20.

High-temperature Isotropic Coatings (HTI). Maltese cross formation had been reported with HTI coatings (deposited at 1900°C from methane) when the BAF was as low as 1.08 (GA-8270). On the basis of this initial work, it appeared that the presence or absence of a Maltese cross might make a satisfactory test for rejecting or accepting coated particles. Considerable effort was expended in an attempt to establish the range of BAF uncertainty that might be involved in such a procedure.

Particles from 26 batches with well-determined BAF (two or more measurements) were mounted and compared. Cluster mounts containing four to 25 particles were used with the standard polishing procedure. An example of a cluster mount containing 25 particles from five batches is shown in Fig. V.1. Low magnification (25X) was used in all cases in order to optimize the Maltese cross intensity (GA-8270). The results of the work are shown in Fig. V.2. Each batch is identified along with its BAF shown as a range of ± 1 standard deviation about the average measured value.

From examination of the results, it was concluded that for HTI coatings the absence of a Maltese cross indicates that the BAF is probably less than 1.16. The presence of a cross indicates that the BAF is greater than 1.05. In view of these conclusions, the use of this optical technique for BAF discrimination in the range of interest (1.08 to 1.12) does not look promising. However, the presence or absence of a Maltese cross in PyC coatings with low BAF values of 1.08 to 1.10 may be indication of significant PyC structural differences. A limited effort will be continued to explore this possibility with regard to the radiation stability of PyC coatings.

<u>BATCH NO.</u>	<u>BAF</u>
HETK 4659	1.07
HETK 4653	1.07
HETK 4695	1.09
HETK 4689	1.09
HETK 4690	1.11

Fig. V.1--Cluster mount displaying five particles from each of five batches; note weak Maltese cross formation in particles of batches HETK-4653 and HETK-4689 (25X)

Fig. V.2--Effect of BAF on Maltese cross formation in HTI pyrocarbon on spherical particles

Mechanism of Maltese Cross Formation. Gray and Cathcart (Ref. 3) have proposed that the Maltese cross arises from optical anisotropy of the PyC crystallites. However, they did not rule out the possibility that the effect may be due to regular microscopic roughness of the polished surface. For instance, regular furrowed surfaces in isotropic materials, such as cubic metals, can produce optical effects under polarized light that are similar to those produced by optically anisotropic materials with plane surfaces (Refs. 4,5). Gray and Cathcart favored the Maltese cross mechanism based on intrinsic optical anisotropy, because optical and electron microscopy of PyC had not revealed a roughness typical of anodic aluminum oxide surfaces, which do show anisotropy due to roughness. However, the observations of the present work can best be explained by the assumption that the Maltese cross arises from the interaction of polarized light with a textured PyC surface containing microcracks or grooves parallel to the deposition plane.

This conclusion is supported by four experimental observations: First, the formation of a Maltese cross was apparently sensitive to the structure of the PyC as well as BAF. This was illustrated by the absence of a cross in LTI coatings having a BAF as high as 1.20 and the rather wide range of BAF (1.08 to 1.16) in HTI coatings where a Maltese cross may or may not be seen. The use of the cluster mount technique largely removed the possibility that these observations arose from variations in metallographic preparation or photography. One obvious mechanism by which variations in the PyC structure could influence optical properties would be in the formation of microcracks or grooves during polishing.

Second, the Maltese cross becomes more intense with decreasing magnification (GA-8270). One explanation for this lies in the fact that the numerical aperture of the metallograph objective lens decreases with decreasing magnification. Therefore the maximum angle of incidence of light on the specimen also decreases with decreasing magnification. Relatively deep narrow grooves separated by planar surfaces might be expected to contribute more to the elliptical polarization of plane polarized light necessary for cross formation, when incident light rays are more nearly parallel (i.e., lower magnification).

Third, between parallel polarizers PyC coatings that are optically active show characteristics that may be attributed either to intrinsic anisotropy or regular grooving of the surface. However, observations indicate that if the effect is due to a grooved surface, the groove axes must be preferentially oriented parallel to the deposition plane.

Fourth, electron microscopy was conducted with several as-polished PyC coatings. A regular texture of discontinuous microcracks or grooves with preferred orientation parallel to the deposition plane was observed at 7000X on two HTI coatings with BAF of 1.20 and 1.15. One HTI and one LTI coating with BAFs of 1.05 and 1.00, respectively, were featureless at 7000X. While these observations do not prove there is a correlation between surface texture and Maltese cross formation, they provide good support for such a conclusion.

Modification of the Standard X-ray BAF Technique

One of the time-consuming steps in the standard BAF measuring technique involves densitometer scanning of X-ray film and processing the resulting data to obtain a pole figure. A new type of densitometer that has been designed and built promises to reduce the time necessary to scan a film and produce a pole figure from about 1-1/4 hr to 3 min. The instrument (called a circular scanning densitometer) integrates the photographic density across an X-ray diffraction ring and plots the data for each point around the ring. The background is automatically subtracted from the integrated measurement, so that a usable pole figure is produced directly. Another important feature of the device is that the point of maximum intensity on the diffraction ring can be easily and accurately located. This important task is more difficult with the standard densitometer.

Calibration of the new device is in progress. Initial results indicate that in the BAF range of 1.00 to 1.50 there is about a 1 to 1 correlation between BAF measured by the standard technique and that measured using the circular scanning densitometer.

ANALYTICAL STUDIES OF COATING DESIGN

BISO Coating Design Calculations

Studies of BISO-coated particle integrity are being conducted using the Prados-Scott model (Ref. 6). The model is used to calculate coating stresses primarily resulting from the effects of fuel burnup and the flux of fast neutrons.

Some of the coated particle parameters were brought up to date. Table V.3 summarizes the changes in the coated particle parameters that were used to evaluate the integrity of BISO-coated particles under HTGR conditions. Using the new parameters in the Prados-Scott model (S-5 program), the particles easily survive HTGR conditions. Failure does not occur at a fast flux of 1×10^{22} n/cm² (the program cutoff point) even at high burnup. Only two failure locus data points were obtained from the computer runs before the cutoff point. These were both for the fertile particle at high burnup and low fast flux (see Fig. V.3). The primary reason for the improved performance is the increased creep coefficient. Figure V.3 also shows that using the new parameters with zero creep results in slightly earlier failure than when the old parameters are used (probably because of the higher Young's modulus). The model predicts failure in fissile and fertile particles for the old parameters and for the new parameters with zero creep.

Several runs were made to evaluate the influence of the thickness of the isotropic layer on the failure loci, holding all other parameters (new set of parameters) constant. The loci were computed for ten different coating thicknesses for both the fertile and fissile particle. The loci, shown in Figs. V.4 and V.5, reveal an improved performance at high fast flux and low burnup when thin coatings are used. Even under HTGR conditions, the model indicates that fast flux is the major contributor to coating failure and that a reduction of coating thickness from the present reference values will improve their

Table V.3
COATED PARTICLE PARAMETERS USED IN S-5^a

Parameter	Old Value	New Value
Isotropic tensile strength, psi	30,000	50,000
Isotropic Young's modulus, psi	2×10^6	4×10^6
Isotropic thermal expansion coefficient, $(^\circ\text{C})^{-1}$	8.2×10^{-6}	5×10^{-6}
Isotropic creep coefficient, $(\text{nvt})^{-1} (\text{psi})^{-1}$	1×10^{-27}	2×10^{-27}
Isotropic Poisson's ratio	0.25	No change
Fuel Young's modulus, psi	2.9×10^7	No change
Fuel Poisson's ratio	0.30	No change
Fuel thermal expansion coefficient, $(^\circ\text{C})^{-1}$	1.58×10^{-5}	No change
Buffer fractional porosity	0.50	No change
Fuel fractional porosity	0.06	No change
Bacon anisotropy factor, isotropic coating	1.10	No change
Fractional linear expansion rate ^b of isotropic coating in parallel crystal direction due to radiation damage, $(10^{20} \text{ nvt})^{-1}$	-0.0004	No change
Fractional linear expansion rate ^b of isotropic coating in perpendicular crystal direction due to radiation damage, $(10^{20} \text{ nvt})^{-1}$	0.0008	No change

^aCore diameter: fissile, 200 μ ; fertile 400 μ . Buffer thickness: fissile, 65 μ ; fertile, 70 μ . Isotropic thickness: fissile, 55 μ ; fertile, 70 μ . Ratio of Th to U: fissile, 4.25; fertile, all Th.

^bThe Prados-Scott model requires the use of linear dimensional change rates for the PyC. However, as Bokros has shown (Ref. 7), the dimensional change rate is not linear with fast dose.

Fig. V.3--Failure loci of BISO-coated particles, S-5 program

Fig. V.4--Influence of outer coating thickness on failure loci of BISO-coated fissile particle, S-5 program

Fig. V.5--Influence of outer coating thickness on failure loci of BIS0-coated fertile particle, S-5 program

performance. The model indicates that the optimum outer coating thickness is between 10 and 30 μ for the fissile particle and between 10 and 50 μ for the fertile particle.

Optimizing the coating thickness with this model requires at least a moderately accurate understanding of fission-gas-pressure generation and irradiation-induced dimensional changes, since these effects influence the selection of coating thickness in opposing senses. In contrast, optimizing with the figure of merit (Ref. 8) model essentially involves only the determination of the optimum trade-off between free volume for fission gas and the thickness of the load-bearing shell for the containment of fission-gas pressure, and it neglects irradiation-induced dimensional changes. As a result, inner and outer coating thicknesses can be optimized on a relative basis, without any knowledge of the extent of fission-gas pressure. Even if uncertainties in fission-gas pressure and irradiation-induced dimensional changes presently preclude an accurate optimization of the BISO coating, the results shown in Figs. V.4 and V.5 indicate that the effects of fast flux should play a significant role in the optimization.

TRISO Coating Design Calculations

Studies of TRISO-coated particle fuel integrity are being conducted using a mathematical model. A metallographic cross section and a schematic representation of a spherical, TRISO-I-coated fuel particle are shown in Fig. V.6. The mathematical model used in these studies is due to Kaae (Refs. 9, 10). The Kaae model is an analysis of coating stresses induced by the build-up of fission-gas pressure, anisotropic radiation-induced dimensional changes in the outer pyrocarbon (PyC) layer, and differential thermal expansion of the SiC and PyC layers. The analysis also accounts for the influences of transient and steady-state radiation-induced creep in the PyC layer.

The primary objective of these studies is to evaluate the influence of pertinent parameters (e.g., environmental, geometric, and physical and mechanical properties) on the mechanical integrity of the coated particles using the Kaae model computer program as an aid in the optimization of the coated particle design.

As a starting point, the initial set of input parameters shown in Table V.4 was established. The fast-flux exposure was allowed to increase in steps of 1.0×10^{20} n/cm² in the computer program. Coating stresses were calculated for each step. The resulting coating-stress history for the initial input parameters was reported in Ref. 11, Fig. 3.5, and is reproduced here in Fig. V.7.

The PyC initially densifies, which causes the rapid increase in the PyC tensile stress and the SiC compressive stress. These stresses tend to be relieved by creep in the PyC. Further increases in stress in the PyC are caused by radial expansion and tangential contraction in the PyC, which begins at about 1.5×10^{21} n/cm². The change in slope at about 4×10^{21} n/cm² is due to a linear extrapolation of the PyC dimensional change data and the increasing internal fission-gas pressure at high burnup. The maximum tensile stress in the PyC is 33,600 psi and the maximum compressive stress in the SiC is 94,200 psi.

Fig. V.6--Model of TRISO-I-coated fuel particle and corresponding coated particle

Table V.4

INITIAL KAAE MODEL INPUT PARAMETERS FOR TRISO-COATED FISSILE PARTICLES

Core diameter, μ	200
Buffer thickness, μ	50
SiC thickness, μ	15
PyC thickness, μ	50
Th in heavy metal, %	75
U in heavy metal, %	25
Theoretical buffer density, g/cm ³	2.2
Buffer density, g/cm ³	1.2
UC ₂ density, g/cm ³	11.3
ThC ₂ density, g/cm ³	8.6
Molar ratio of fission gas to fissions	0.6
SiC Poisson's ratio	0.33
SiC Young's modulus, psi	60x10 ⁶
PyC Poisson's ratio	0.33
PyC Young's modulus, psi	4x10 ⁶
Temperature of irradiation, °C	900
Temperature at which PyC (HTI) is deposited, °C	2000
PyC coefficient of thermal expansion, °C ⁻¹	5.0x10 ⁻⁶
SiC coefficient of thermal expansion, °C ⁻¹	4.5x10 ⁻⁶
Transient, neutron-induced creep constant in PyC (HTI), (n/cm ² psi) ⁻¹	2.5x10 ⁻²⁷
Steady-state, neutron-induced creep constant in PyC (HTI), (n/cm ² psi) ⁻¹	1.0x10 ⁻²⁷
Neutron-induced dimensional changes in PyC (HTI)	Bokros curves (Ref. 11), Case C

NOTE: Burnup - fast-flux relationship, straight line from (0,0) to 20% FIMA, 7×10^{21} n/cm².

FIG. V.7 -- Calculated coating stresses in base case TRISO-coated fissile particle taken from Ref. 11. Calculations based on Knae model using initial fissile input parameters. Curve for PTI omitted, since PTO is more severe stress. PTO = tangential stress in PyC at coating outer surface, PTI = tangential stress in PyC at coating inner surface, and TS = tangential stress in SiC at coating inner surface. PyC initially undergoes densification causing rapid increase in PTO and rapid decrease in TS. These stresses tend to be relieved by creep in PyC. Radiation-induced radial expansion and tangential contraction in PyC at $\sim 1.5 \times 10^{21} \text{ n/cm}^2$ cause further increases in stress. Change in slope for TS curve at $\sim 4 \times 10^{21} \text{ n/cm}^2$ is due to increase in internal fission gas pressure and use of a linear extrapolation of dimensional change data

The initial input parameters were used to evaluate the influence of SiC and PyC coating thicknesses on coating stress. This work was reported in Ref. 11, Fig. 3.3, along with an investigation of the influence of using PyC dimensional change curves A, C, and E (Ref. 11, Fig. 3.4).

Several of the parameters of Table V.4 were changed to establish their influence on coating stresses. The results are shown in Ref. 11, Fig. 3.5. It was observed that the most sensitive parameters were the dimensional change behavior of the PyC and its steady-state irradiation-induced creep constant.

A series of calculations was performed to evaluate the coating stresses when the PyC coating is eliminated leaving only the SiC coating for a structural shell. Coating stress histories were calculated for various buffer and SiC thicknesses. The maximum tensile stress in the SiC coating during the particle lifetime is plotted versus SiC thickness in Fig. V.8. It is noted that removal of the PyC layer results in significant tensile stresses in the SiC, even when large buffer coatings are used.

Following these initial studies, an improved and more realistic set of input parameters have been compiled (Table V.5). Before the adoption of a standard buffer density of 1.1 g/cm^3 and an SiC thickness of 20μ , a coating stress history was calculated for an interim reference design. The results were reported in Ref. 11, Fig. 3.7, and are reproduced here in Fig. V.9. A significant reduction in coating stresses from those for the initial set of input parameters (Fig. V.7) is noted. This is primarily due to the increased steady-state creep coefficient.

A series of calculations was performed to determine the effects of reducing the second coating (SiC) to a very thin layer and/or greatly reducing its Young's modulus. The purpose of these calculations was to determine calculated stresses in a BISO coating using the Kaae model. Such stresses can be compared to those of a TRISO coating to determine the influence of adding the SiC layer. The results shown in Figs. V.10, V.11, and V.12 reveal that the maximum PyC stress is essentially the same when the influence of the second layer (SiC) is reduced either by reducing its thickness or by reducing its modulus. However, PyC stress is significantly reduced from that of the interim reference design (Fig. V.9).

The establishment of the standard input parameters created the need for another investigation of the effects of SiC and PyC coating thicknesses on the coating stresses. Accordingly, several coating stress histories were calculated. A portion of the results for the fissile-coated particle are shown in Fig. V.13. The stress histories of Fig. V.13 are for the extremes in coating thicknesses that were considered and were chosen to demonstrate the trends resulting from changing the SiC and PyC coating thicknesses. The characteristic shape of these curves is similar to that of the previous stress history curves, but the characteristics are emphasized in varying degrees. For example, in parts A and B of Fig. V.13 the early densification of the PyC has little influence on SiC stress because the PyC layer is thin. Maximum PyC stress is reached at about $5 \times 10^{21} \text{ n/cm}^2$ and then remains constant. It is also noted that the maximum stress in the PyC is very insensitive to coating design, but that increasing the SiC thickness reduces the magnitude of

Fig. V.8 --Calculated maximum SiC stresses during lifetime of TRISO-coated fissile particles showing effect of reducing thickness of outer coating (PyC layer) to very small value and varying buffer and SiC thicknesses. Calculations based on Kaae model using initial fissile input parameters (see Table V.5), except for following: Buffer and SiC thicknesses = as noted, PyC thickness = 10^{-5} μ

Table V.5

STANDARD KAAE MODEL INPUT PARAMETERS FOR TRISO-COATED PARTICLES

Core diameter, μ	200 fissile, 400 fertile
Buffer thickness, μ	50 fissile, 50 fertile
SiC thickness, μ	20 fissile, 20 fertile
PyC thickness, μ	50 fissile, 60 fertile
Th in heavy metal, %	81 fissile, 100 fertile
U in heavy metal, %	19 fissile, 0 fertile
Theoretical buffer density, g/cm ³	2.2
Buffer density, g/cm ³	1.1
UC ₂ density, g/cm ³	11.3
ThC ₂ density, g/cm ³	8.6
Molar ratio fission gas to fissions	0.5
SiC Poisson's ratio	0.33
SiC Young's modulus, psi	60x10 ⁶
PyC Poisson's ratio	0.33
PyC Young's modulus, psi	4x10 ⁶
Temperature of irradiation, °C	1260
Temperature at which PyC (HTI) is deposited, °C	1800
PyC coeff. of thermal expansion, °C ⁻¹	5.4x10 ⁻⁶
SiC coeff. of thermal expansion, °C ⁻¹	5.5x10 ⁻⁶
Transient neutron-induced creep constant in PyC (HTI), (n/cm ² psi) ⁻¹	2.5x10 ⁻²⁷
Steady-state neutron-induced creep constant in PyC (HTI), (n/cm ² psi) ⁻¹	2.0x10 ⁻²⁷
Neutron-induced dimen. changes in PyC (HTI)	Bokros curves (Ref. 11), Case C

NOTE: Burnup - fast-flux relationship: fissile, realistic curve (Ref. 11), from (0,0) to 20% FIMA, 8×10^{21} n/cm², where burnup rate decreases with neutron dose; fertile, realistic curve (Ref. 11), from (0,0) to 7% FIMA, 8×10^{21} n/cm², where burnup rate increases with neutron dose.

Fig. V.9--Calculated coating stresses in interim reference design, TRISO-coated fissile particle taken from Ref. 10. Calculations based on Kaae model using standard fissile input parameters (Table V.6), except that buffer density of 1.2 g/cm^3 and SiC thickness for 15μ used here. PTO = tangential stress in PyC at coating outer surface, TS = tangential stress in SiC at coating inner surface

Fig. V.10--Calculated coating stresses in interim reference design, TRISO-coated fissile particle showing effect of very small Young's modulus for second coating (SiC layer). Calculations based on Kaae model using standard fissile input parameters (Table V.6) except for the following: Buffer density = 1.2 g/cm³, SiC thickness = 15 μ , and SiC Young's modulus = 1.0 psi.

Fig. V.11---Calculated coating stresses in interim reference design, TRISO-coated fissile particle showing effect of reducing both thickness and Young's modulus of second coating (SiC layer). Calculations are based on Kaae model using standard fissile input parameters (Table V.6) except for following: Buffer density = 1.2 g/cm^3 , SiC thickness = $10^{-5} \mu$, and SiC Young's modulus = 1.0 psi

Fig. V.12--Calculated coating stresses in interim reference design, TRISO-coated fissile particle showing effect of reducing thickness of the second coating (SiC layer). Resulting compressive stresses in SiC were very large and were omitted here. Calculations based on Kaae model using standard fissile input parameters (Table V.6) except for following: Buffer density = 1.2 g/cm^2 and SiC thickness = $10^{-5} \mu$

Fig. V.13---Calculated coating stresses in TRISO-coated fissile particles showing coating stress history for various combinations of SiC and PyC coating thicknesses. Parts A-D show the extreme combinations of SiC and PyC thicknesses considered. Part E is selected reference fissile design. Calculations are based on Kaas model using the standard fissile input parameters except for noted SiC and PyC thicknesses. Note changes in scale on ordinate between parts

Fig. V.13--Continued

CASE 57

5 MICRONS SIC

100 MICRONS PYC

Fig. V.13--Continued

Fig. V.13--Continued

FERTILE - CASE 64 - 200.50.20.60

Fig. V.13--Continued

stresses in the SiC layer. Thin PyC coatings allow the SiC to go into tension, and thick PyC coatings maintain the SiC in compression. The coating parameters of part E of Fig. V.13 were selected for the reference TRISO-I design. Additional analytical studies are being conducted on the TRISO-II design, which incorporates the SiC layer between two PyC layers rather than between the buffer and PyC layers as in the TRISO-I design.

Maximum tensile and compressive coating stresses in the fissile-coated particle are shown in more detail in Figs. V.14 and V.15. In addition, these figures put some of the above observations of Fig. V.13 in clear perspective.

Additional calculations for the fissile particle were performed to simultaneously evaluate the effects of increasing the core diameter and decreasing the neutron penalty associated with the use of SiC. Several stress history calculations were conducted for various combinations of core diameter and SiC thickness. As the core diameter was increased, the SiC thickness was increased to a value that would result in either the same SiC neutron penalty as in the reference coated particle or a prescribed fraction thereof. To establish the required SiC thicknesses, it was assumed that the neutron penalty associated with SiC is directly proportional to the ratio of SiC coating volume to core volume. This ratio has the value of 1.538 for the reference fissile particle. Maximum and minimum coating stresses during the coated particle lifetime were plotted versus core diameter in Fig. V.16 for several relative neutron penalties. It is seen that maximum tensile stress in the SiC layer increases with increasing core diameter, even though the SiC coating thickness is increased to maintain constant neutron penalty. However, compressive stresses in the SiC are somewhat relieved. These effects are primarily due to maintaining constant PyC thickness while the particle diameter is increasing. This reduces the thickness-to-diameter ratio of the PyC layer and thus reduces its capacity to drive the SiC layer into compression. (However the buffer layer is another contributor to this effect, since its effectiveness in accommodating fission gases is reduced by maintaining its thickness constant.) It appears that, if a reduction of the SiC neutron penalty is made, the reference 200- μ core diameter should be maintained (unless appropriate increases in buffer and PyC thicknesses can be made to offset tensile stresses in the SiC).

The calculations to determine the effects of SiC and PyC coating thicknesses were repeated for the standard input parameters for the fertile coated particle. Some of the stress history curves are shown in Fig. V.17 along with the curve for the reference TRISO-I design for the fertile-coated particle. These curves and the plots of maximum and minimum stresses (Figs. V.18 and V.19) reveal trends that are quite similar to those for the fissile particle. However, the stresses are somewhat more severe in the fertile-coated particle, for equal coating thicknesses.

Studies of the effects of core diameter and SiC neutron penalty were conducted for the fertile particle. These studies were not done in as much detail as those for the fissile particle, but included the effects of core diameter for (1) constant SiC thickness and (2) constant SiC neutron penalty. The neutron penalty was held constant using the previously described method used in the studies of the fissile particle. The ratio of SiC coating volume to core volume is 0.50725 for the reference fertile particle. The results,

Fig. V.14--Calculated maximum tensile and compressive stresses in SiC layer during lifetime of TRISO-coated fissile particle showing effect of varying thickness of SiC and PyC layers. Calculations based on Kaae model using standard fissile input parameters (Table V.6) except for noted SiC and PyC thicknesses

Fig.V.15--Calculated maximum stresses in outer coating (PyC layer) during lifetime of TRISO-coated fissile particle showing effect of varying thickness of SiC and PyC layers. Calculations based on Kaae model using standard fissile input parameters (Table V.6) except for noted SiC and PyC thicknesses

Fig. V.16--Calculated maximum tensile and minimum compressive stresses during lifetime of TRISO-coated fissile particle showing effects of increasing core diameter at constant SiC neutron penalty and SiC thickness. Neutron penalty assumed directly proportional to ratio of SiC volume to core volume. Therefore when neutron penalty associated with reference coated particle is used, SiC thickness increases with increasing core diameter so above volume ratio is maintained equal to that of reference coated fissile particle. SiC thickness similarly varied for noted reductions in the volume ratio. Dashed curves show stress versus core diameter relationships for constant SiC thickness (as noted). Calculations based on Kaae model using standard input parameters except for noted variations in SiC thickness and core diameter

Fig. V.17--Calculated coating stresses in the TRISO-coated fertile particles showing coating stress history for various combinations of SiC and PyC coating thicknesses. Parts A-D show the extreme combinations of SiC and PyC thicknesses considered. Part E is for selected reference fertile design. Calculations based on Kaae model using standard fertile input parameters except for noted SiC and PyC thicknesses. Note changes in scale on ordinate between parts

Fig. V.17--Continued

Fig. V.17 --Continued

Fig. V.17--Continued

CASE 57-200M CORE, 50M BUFFER, 20M SIC, 50M PYC

Fig. V.17--Continued

Fig. V.18--Calculated maximum tensile and compressive stresses in SiC layer during lifetime of TRISO-coated fertile particle showing effect of varying thickness of SiC and PyC layers. Calculations based on Kaae model using standard fertile input parameters (Table V.6) except for noted SiC and PyC thicknesses

Fig. V.19--Calculated maximum stresses in outer coating (PyC layer) during lifetime of TRISO-coated fertile particles showing effect of varying thickness of SiC and PyC layers. Calculations based on Kaae model using standard fertile input parameters (Table V.6) except for noted SiC and PyC thicknesses

shown in Fig. V.20, indicate that significant reductions in the compressive stresses in the SiC coating can be achieved by increasing the core diameter, whereas tensile stresses are not influenced until the core diameter exceeds 500 μ . This effect is observed to a lesser extent with constant SiC thickness than with constant neutron penalty, but is significant in both cases. It is therefore apparent that increasing the fertile core diameter would be a more fruitful means for reducing the neutron penalty than increasing the fissile core diameter. Increasing the fertile core diameter without increasing the coating thicknesses would increase the heavy metal content in the coated particle, thereby mitigating fuel loading problems in the fuel elements. Furthermore, the larger fertile coated particle would accentuate size separation of fissile- and fertile-coated particles in reprocessing and recovery operations.

Irradiation tests of TRISO-coated fuel particles have been conducted in the P13F, P13H, and P13J capsules. Stresses have been calculated for TRISO-I coatings for each of these experiments. Most of the calculations have been reported in Ref. 11, where observed failure rates were shown to correlate very well with the Kaae model. The calculations have been repeated recently, using revised dimensional change and creep parameters for the PyC layer. Calculations using the revised dimensional change parameters are incomplete. However, calculations using a steady-state creep coefficient for LTI PyC of $1.0 \times 10^{-27} \text{ (psi n/cm}^2\text{)}^{-1}$ have been completed for P13J. This is considered to be an upper limit for the creep coefficient. A creep coefficient of $2.0 \times 10^{-27} \text{ (psi n/cm}^2\text{)}^{-1}$ (the accepted value for HTI PyC) was used in the calculations reported in Ref. 11. The resulting calculated maximum tensile and compressive stresses are shown in Table V.6 for both creep constants. The revised stresses no longer correlate with failure rates as well as the original calculations. The failure rate is highest for case 8, but the revised stresses are no longer the highest in case 8.

FISSION-PRODUCT RELEASE

Postactivation fission-product release measurements were conducted on unirradiated samples from lots 3332-101E, 3332-131E, 3332-145E, and 3325-123E. Three samples from each batch (approximately 5000 particles in each sample) were leached in HNO_3 and were then given light irradiations (10^{14} fissions) in the TRIGA reactor. One sample each was annealed at 1400°, 1600°, and 1800°C. During the anneal, the strontium and barium nuclides released from the particles were collected in the graphite crucibles. Xenon nuclides were collected in charcoal traps. The anneals were interrupted at time intervals of about 1, 3, 5, 8, and 20 hr to change the crucibles. The exposed crucibles were gamma-counted immediately to determine Sr^{91} release and again after several days (after sufficient time for La^{140} to grow in) to determine Ba^{140} release.

Table V.7 is a summary of the release data. The xenon release data (not included in Table V.7) indicate that no significant failure of coatings occurred during the annealing tests. If both the SiC and the outer pyrolytic carbon coatings had failed, gross xenon release would have occurred.

Fig. V.20--Calculated maximum tensile and minimum compressive coating stresses during lifetime of TRISO-coated fertile particle showing effect of varying core diameter both for constant SiC thickness and constant neutron penalty. Where neutron penalty is maintained constant, SiC thickness is increased with increasing core diameter so ratio of SiC volume to core volume is maintained constant. Calculations based on Kaae model using standard fertile input parameters (Table V.6) except for noted variations in SiC thickness

Table V.6

P13J CALCULATED COATING STRESSES

Case	Max. PyC Stress (10 ³ psi)		Min. SiC Stress (10 ³ psi)		Max. SiC Stress (10 ³ psi)		Observed Failures ^b (%)
	K _{ss} = 1 ^a	K _{ss} = 2 ^a	K _{ss} = 1 ^a	K _{ss} = 2 ^a	K _{ss} = 1 ^a	K _{ss} = 2 ^a	
1	35.1	18.5	-7.0	-3.1	27.3	39.3	1
2	35.0	18.4	-4.7	-2.1	16.2	23.5	0
3	34.5	18.2	-2.4	-1.0	13.0	17.2	0
4	37.5	19.7	-38.5	-20.0	0.1	23.0	0
5	39.0	20.4	-95.7	-31.0	0.2	0.2	
6	34.8	18.3	-5.5	-2.7	4.5	9.9	0
7	34.8	18.3	-4.7	-2.3	3.6	8.2	0
8	35.1	18.5	-8.2	-3.7	17.3	20.4	6
9	35.0	18.4	-5.4	-2.5	10.2	17.5	0.7
10	34.5	18.2	-2.9	-1.3	8.8	13.0	0
11	35.0	18.4	-9.8	-4.9	6.6	16.2	0.2
12	34.8	18.4	-6.4	-3.2	3.6	9.4	0
13	34.6	18.2	-3.6	-1.7	3.7	7.4	0

^aK_{ss} = steady-state, neutron-induced creep coefficient in PyC (10²⁷ n/cm² psi)⁻¹.

^bBy microscopic postirradiation examination (from Ref. 6).

Table V.7

FISSION-PRODUCT RELEASE OF SiC-FUEL PARTICLES

Lot No. and Particle Description ^a	Coating Thickness (μ)				Anneal		Fractional Release	
	Buffer	SiC	Iso	Total	Temp (°C)	Time (hr)	Ba ¹⁴⁰	Sr ⁹¹
3332-101E (Th,U)C ₂ Th/U=3.0 150-250 μ	58	13	40	111	1400	20	1.1x10 ⁻⁴	5.4x10 ⁻⁴
					1600	20	7.2x10 ⁻⁴	2.0x10 ⁻³
					1800	20	1.5x10 ⁻³	2.2x10 ⁻³
3332-131E (Th,U)C ₂ Th/U=3.0 150-250 μ	34	30	10	74	1400	21	2.1x10 ⁻⁴	4.0x10 ⁻³
					1600	20	8.0x10 ⁻⁴	5.3x10 ⁻³
					1800	21	3.2x10 ⁻²	2.8x10 ⁻³
Unleached sample					1400	20	3.6x10 ⁻⁴	2.8x10 ⁻³
					1600	20	4.7x10 ⁻³	8.8x10 ⁻³
					1800	20	2.6x10 ⁻²	3.1x10 ⁻²
3332-145E (Th,U)C ₂ Th/U=3.0 150-250 μ	34	40	9	83	1400	21	1.2x10 ⁻⁵	<1.0x10 ⁻⁵
					1600	21	1.7x10 ⁻⁵	1.4x10 ⁻⁴
					1800	20	6.5x10 ⁻⁴	2.0x10 ⁻⁴
3325-123E (Th,U)C ₂ Th/U=1.6 150-250 μ	41	30	10	81	1400	20	7.4x10 ⁻⁴	5.3x10 ⁻³
					1600	20	9.5x10 ⁻³	7.1x10 ⁻³

^aAll samples were leached before activation in the TRIGA reactor, except as noted.

Two of the samples (from lots 3325-123E and 3332-131E) exhibited barium and strontium releases higher than expected for TRISO particles. Both of these samples also showed higher than expected releases of Xe^{135} indicating significant levels of contamination in the outer isotropic pyrocarbon layer or an unusually high fraction of failed particles. For one of these samples (3332-131E) the metallic releases also showed a greater temperature dependence than is normally observed for the "contamination release" of TRISO particles. The two remaining samples (3332-101E and 3332-145E) showed relatively low levels of release at all temperatures with little temperature dependence.

The data in Table V.8 show no apparent difference in the behavior of leached and unleached particles from lot 3332-131E.

PHASE BEHAVIOR OF THE Th-Si-C AND U-Si-C SYSTEMS AT HIGH TEMPERATURES

Following the addition of a SiC barrier layer to PyC-coated fuel particles, an investigation of the actinide (Th or U)-silicon-carbon ternary system was initiated to determine the temperature at which the first liquid phase is formed.

Since there will always be excess carbon present on both sides of the SiC-barrier layer, it is clear that the over-all composition that results from the interaction (if any) between the fuel and the SiC layer must have a minimum carbon content that corresponds to that along the stable quasibinary join between the metal carbide (ThC_2 or UC_2) and SiC. The species $\text{ThC}_2 + \text{SiC}$ can be shown to be thermodynamically stable with respect to the equivalent composition ThSi (or Th_3Si_5) plus elemental carbon, thus ensuring that they will be the coexisting phases. Thus, no (low melting) liquid metallic phase containing primarily U or Th and Si can be formed. A low melting eutectic resulting from the interaction of the actinide metal carbide and silicon carbide and carbon is therefore the only source of concern.

Accordingly, preliminary experiments have been undertaken to investigate the actinide (U or Th) carbide-silicon carbide systems in the high carbon region beyond the quasibinary join. Thermal analysis studies of the ternary systems Th-Si-C and U-Si-C are under way to determine whether a eutectic exists at a composition of 50-50 mole-% SiC and MC_2 ($M = \text{U}$ or Th). Initial experiments in the Th-Si-C system indicated the possible existence of a liquid phase at about 2300°C , some 200°C below the binary eutectic for $\text{ThC}_2\text{-C}$. Rapid vaporization of silicon at the higher temperatures made it difficult to obtain good thermal analysis curves. Future work will be conducted using sealed graphite crucibles to retard the loss of silicon by vaporization.

DIFFUSION OF STRONTIUM AND BARIUM IN SiC-COATED FUEL PARTICLES

A study was begun to determine the diffusion coefficients for strontium and barium in SiC using TRISO fuel particles. The method is to measure the release of strontium and barium in long-term annealing experiments. Diffusion coefficients can be derived from the release data.

Preliminary analyses of the two samples selected for these measurements from lots 3332-101E and 3592-53E were conducted using radiomicrography. Several interesting facts were noted when the radiomicrographs were examined microscopically.

1. The appearance of some of the SiC layers indicated that defects may be present in some of the coatings.
2. One particle contained only an isotropic pyrolytic carbon layer surrounding the kernel.
3. Some dense material, which appeared to be particle fragments, was detected.

These findings are important in that they may explain the level of metallic-fission-product releases observed with SiC-coated particles. In earlier work barium and strontium fractional releases of the order of 10^{-4} to 10^{-3} have been attributed to uranium contamination in the outer pyrolytic carbon layer. However, one or two defective particles in a sample of 5000 particles could explain the observed levels of metallic-fission-product release. It appears, therefore, that if release from intact SiC-coated particles is to be measured in annealing experiments, a smaller number of particles (about 20) should be used and radiomicrography of the particles selected should also be conducted in an attempt to eliminate defective particles.

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