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AEC RESEARCH AND
DEVELOPMENT REPORT

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GA-8420

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

QUARTERLY PROGRESS REPORT
FOR THE PERIOD ENDING
DECEMBER 31, 1967

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

January 29, 1968

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Gulf General Atomic Incorporated

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

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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 (PCRV) 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.

PCRV Model No. 2 Testing

The sustained pressure testing of Model No. 2, which was initiated on August 7, 1967, has been continuing during this period. The internal oil pressure has been maintained within $\pm 5\%$ of 585 psig (NWP) at ambient temperature. The oil temperature has been monitored during the pressure hold period and is plotted on Fig. 1.1 for the first 90 days of the test. The oil temperature was fairly well constant at about 76°F for the first 40 days, but has been decreasing at such a rate that within the next 50 days the temperature has dropped by 9°F to 67°F due to the onset of the cold weather period. During the first 40 days, the temperature of the exterior concrete of the vessel varied with respect to the vessel height, the temperature of the vessel top head being higher than the middle and bottom portion by approximately 6° to 7°F. This difference in temperature decreased to about 2°F during the next 50 days. The average external temperature of the middle concrete region has always been lower than the internal oil temperature by approximately 3°F during the 90-day period.

During the pneumatic overpressure and gas release tests reported in the previous quarterly report (GA-8270), the roof of the silo enclosure around the model was removed as a precautionary measure. The silo roof was not re-installed until November 15, 1967. The temperature differentials observed above are attributable primarily to the direct exposure of the top head region to the weather.

Fig. 1.1--Strain versus log time for a circumferential concrete gage at inner midplane

PCRV Model No. 2 Design and Analysis

Sustained Pressure Test Results. Data obtained during the first 90 days of the 585-psig sustained pressure test have been reduced by computer and preliminary results are presented in Figs. 1.1 through 1.5. The sustained pressure test at ambient temperature is being conducted in order to obtain creep information for a period much longer than the 22-day 585-psig sustained pressure test at elevated temperature previously completed. Approximately 5-1/2 months elapsed between the completion of the elevated temperature and the start of the ambient temperature 585-psig sustained pressure tests, and during this time interval, the vessel underwent several test phases as follows: (1) temperature cooldown from 140°F to ambient, (2) several permeation tests, (3) pneumatic overpressure and gas release tests up to 1150 psig, and (4) hydraulic overpressure test series to 1200 and 1500 psig. Although the vessel had been subjected to overpressures up to 1500 psig (2.13 times reference pressure), which resulted in extensive concrete cracking as shown in Figs. 1.6 and 1.7, the structural integrity of the vessel is intact and the prestressing level is such that the vessel is in net compression at 585-psig internal pressure.

Figures 1.1 through 1.5 show strain versus time curves obtained from representative gages in the vessel during the 585-psig sustained pressure tests at both ambient and elevated temperature conditions. Creep curves from the same gages were published in an earlier quarterly report (GA-8038). Reference (zero strain) strain for the curves shown on Figs. 1.1 through 1.5 is taken just before the application of prestressing to the vessel; the positive (+) sign indicates tensile strain, and the negative (-) sign indicates compressive strain. The straight lines shown on Figs. 1.1 through 1.5 were obtained from a least-squares fit to the data from the 22-day 585-psig sustained pressure test at elevated temperature.

Strains measured by gages embedded in the concrete at different locations in the vessel are shown on Figs. 1.1 through 1.3. During the first 20 to 40 days of the 585-psig sustained pressure test at ambient temperature, the concrete strains were observed to increase with time in the same direction as the strain due to pressurization, which in the case of the gages shown on Figs. 1.1 through 1.3, is in the tensile direction. After the initial period of about 20 to 40 days, the strain trend reversed and became compressive as shown on Figs. 1.1 through 1.3. The initial strain behavior is probably due to "creep recovery" resulting from stress reduction of effective prestressing, which other investigators refer to as the "delayed elastic" response due to stress application. It appears from the limited data evaluated to date that this initial strain response terminates between 20 to 40 days and basic creep response takes over. The compressive creep observed in Figs. 1.1 through 1.3 is attributed to the general net compressive condition of the vessel under the combination of 585-psig internal pressure and effective prestressing. The variation in ambient temperature with time (shown on Fig. 1.1 and discussed above) may have some effect on the creep behavior of the vessel; however, the possible effect is minimized, since the temperature drop of about 9°F occurred gradually over a period of 50 days and no significant temperature gradients were developed across the vessel wall and head regions. The strain gages in the vessel are temperature compensated; only mechanically induced strains are shown on Figs. 1.1 through

Fig. 1.2--Strain versus log time for an axial concrete gage at inner top haunch

Fig. 1.3--Strain versus log time for an axial concrete gage at top head near haunch

Fig. 1.4--Strain versus log time for a meridional gage on liner cylinder region

Fig. 1.5--Strain versus log time for a circumferential gage on liner top haunch

Fig. 1.6--Crack patterns after 1200-psig overpressure test

Fig. 1.7--Crack patterns after 1500-psig overpressure test

1.5, and thermal expansion or contraction of the vessel materials are not included.

Figures 1.4 and 1.5 show the measured strains registered by the gages attached to the steel liner. Strain versus log time curves for the same gages were also presented in Figs. 1.11 and 1.12 of an earlier quarterly report (GA-8038). A large difference in initial strain is indicated by both gages at an internal pressure of 585 psig between the ambient and elevated temperature test conditions, the elevated temperature test showing a much larger compressive strain than the ambient temperature test. The strain difference is due mainly to the additional compressive strain developed in the steel liner during the elevated temperature test as a result of the thermal difference ($\Delta T = 50^\circ\text{F}$) across the wall and the geometric restraint offered by the vessel. The concrete strains shown on Figs. 1.1 through 1.3 exhibited relatively small difference in strain between the two test conditions, due in part to the smaller thermal compressive strains developed at the gage locations (see GA-8038, Figs. 1.4, 1.6, and 1.7) and the effect of thermal stress relaxation during the elevated temperature tests.

The strain rates observed during the 585-psig sustained pressure test at ambient temperature are relatively low despite the previous test history of the vessel. Figures 1.6 and 1.7 illustrate the extent of exterior surface cracking of the vessel concrete after overpressure testing to 1200 psig and 1500 psig, respectively.

Ultimate Load Analysis. An ultimate load analysis for PCRV Model No. 2 using an axisymmetric crack analysis procedure has been completed. The analytical results are being evaluated and compared with experimental data obtained during the overpressure tests to 1500 psig.

PCRV Liner Development

The prestressed concrete reactor vessel (PCRV) deforms due to loading, including shrinkage, prestress, creep, heating, and pressurization. These deformations impose general biaxial compressive membrane strains on the PCRV liner during all stages of the reactor life. A test program was initiated to demonstrate the ability of the selected liner and anchor design to accept these loading conditions without developing deformations that could adversely affect the performance of the liner, anchors, thermal barrier, or cooling tubes. The tests consisted basically of loading full-size sections of liner plate that were anchored to concrete, up to and beyond the maximum predicted liner biaxial strains. Provision was made to apply loads normal to the plate surface to simulate the effects of gas pressure on the liner in the presence of voids in the concrete behind the liner or of water pressure behind the liner resulting from postulated cooling tube leakage.

Details of the test setup are shown in Figs. 1.8 and 1.9. The test specimens consisted of a 30-in. by 28-1/2-in. piece of 3/4-in. ASTM-A537 Grade A plate anchored to 3-1/2-in. thick concrete backing and a 3-in. thick steel support plate. There were sixteen 3/4-in.-diam anchor studs on a 7-1/2-in. square pitch restrained by nuts on the lower surface of the support plate. To ensure an even distribution of strain within the test plate and to avoid

Fig. 1.8--PCRV liner biaxial compression test rig, side view

Fig. 1.9--PCR liner biaxial compression test rig, top view

loading the concrete as well as the specimen plate, the shanks of the studs in the concrete were wrapped in Styrofoam to prevent shear loading of the studs. In addition, the specimen plate protruded beyond the concrete by 3/4 in. on all sides, thus permitting loading of the specimen plate alone. An 8-in.-diam void was cast into the backup concrete between the center four anchor studs to permit an investigation of the effects of concrete voids behind the vessel liner. The specimen plate was extensively instrumented in the area of the center four studs. Thirty electrical resistance strain gages were attached to the specimen, generally in back-to-back pairs. Microstrain extensometers were mounted on the specimen plate in both loading directions to measure over-all strains in the plate. Nine lateral deflection transducers were mounted above the specimen plate to measure out-of-plane (buckling) deflections of the plate during loading. In addition, the center four anchor studs were fitted with load cell washers to measure the tensile forces developed in the studs.

As shown in Figs. 1.8 and 1.9, the test rig consisted of three large loading blocks on each edge of the specimen. Biaxial loads were applied to the specimen plate through these blocks by two hydraulic jacks on each of the six tension rods in each direction. The hydraulic jack systems in each direction were independent of each other, thereby permitting independent control of the loads in each direction. An additional hydraulic jack system was mounted below the center of the specimen plate to apply lateral loads to the specimen to simulate the effects of gas pressure on the liner in the presence of an 8-in.-diam void in the concrete behind the liner or of water pressure of 125 psi.

Four specimens were tested, the test sequence for each specimen being similar. The basic test sequence was to load the specimen in increments to approximately the maximum predicted biaxial strain condition and then to apply 125 cycles of lateral load. The biaxial strain was then increased to approximately twice the maximum predicted value, at which strain condition a further 125 cycles of lateral load were applied. The lateral load cycles applied were as follows:

1. Upward load (away from concrete), equivalent to 125 psi water pressure acting over one 7-1/2-in. by 7-1/2-in. square area = 7000 lb.
2. Downward load (into concrete), equivalent to gas pressure of 700 psi on 8-in.-diam void less 125-psi water pressure = 29,000 lb.

Four separate specimens were tested: The first two were plain, nominally flat plate specimens; the third was prepared with a 1/8-in. offset weld along the centerline in the rolling direction; the fourth had a cooling tube welded along the centerline in the rolling direction. Tensile tests of the liner material revealed typical yield and ultimate tensile strengths of 47,000 psi and 78,000 psi, respectively, for both longitudinal and transverse directions. Tensile tests of six anchor studs, welded to pieces of 3/4-in. plate gave a typical tensile strength of 30,000 lb, failure occurring in the shank of the stud in all cases.

Specimen No. 1 was loaded biaxially, in small increments, to an equal biaxial strain of 1400 μ in./in. A visual inspection revealed no buckling, when the plate was unloaded.

Following instrumentation checks, the plate was reloaded, again in small increments, to 1250 $\mu\text{in./in.}$ equal biaxial strain. Under these conditions, 250 cycles of transverse load were applied to the center of the plate. The average strain level in the plate during these cyclic loads was 1400 $\mu\text{in./in.}$ The plate was then unloaded, again with no visual signs of buckling.

Upon reloading the plate, the strain levels were increased to 2500 $\mu\text{in./in.}$ equal strain, whereupon 125 cycles of transverse load were applied. At the conclusion of load cycling, the plate was then further strained to approximately 5500 $\mu\text{in./in.}$ in both directions. At a strain level of 3000 $\mu\text{in./in.}$ and greater, a gross buckle developed and the applied load was removed. A permanent set of 4300 $\mu\text{in./in.}$ remained along with a maximum vertical deflection of 0.830 in. This buckling is believed to have occurred through the loss of effective restraint from one of the concrete anchor studs which had been improperly anchored to the base plate.

Specimen No. 2 was loaded in small increments to a strain level of 1000 $\mu\text{in./in.}$ in the axial direction and 1600 $\mu\text{in./in.}$ in the circumferential direction, the latter being the rolling direction of the plate. Under these strain conditions, 125 cycles of transverse load were applied. No significant strain increases were observed at the conclusion of transverse loading. Upon increasing the strains to 1100 axial and 1800 circumferential, ten transverse load cycles were applied. Again, no significant strain increases were observed. Upon loading the plate to a strain level of 3800 $\mu\text{in./in.}$ axially and 2500 $\mu\text{in./in.}$ circumferentially, 125 lateral load cycles were applied to the center of the plate. Average plate strain under load at the conclusion of this test sequence was 4600 $\mu\text{in./in.}$ axially and 2600 $\mu\text{in./in.}$ circumferentially. No vertical deflections greater than 0.050 in. were detected when the plate was unloaded.

At this point, one of the four central concrete anchor studs was removed, thus creating a local 15 in. by 15 in. stud pattern. With the stud missing, a strain of 1300 $\mu\text{in./in.}$ equal biaxial strain was applied. An additional ten cycles of transverse load were then applied, except that the upward load was increased to 15,000 lb from 7000 lb to investigate the effect of water pressure on the larger, 15-in. square area. No significant strain increases were noted.

The strain level of the specimen plate was raised to 2600 $\mu\text{in./in.}$ equal biaxial, and ten cycles of transverse load, 29,000 lb down, 15,000 lb up, were applied. The plate yielded further due to this loading increasing the axial strain 500 $\mu\text{in./in.}$ and the circumferential strain 300 $\mu\text{in./in.}$

The specimen plate was then unloaded and inspected. The maximum residual vertical deflection was 0.050 in. and the permanent set was 3500 $\mu\text{in./in.}$ axially and 1800 $\mu\text{in./in.}$ circumferentially.

Specimen No. 3 contained a 1/8-in. welded offset along the circumferential direction, the rolling direction of the plate. Upon loading to 1250 $\mu\text{in./in.}$ equal biaxial strain, 125 transverse loading cycles were applied. During the cycling operation, the center of the plate exhibited a net downward deflection of 0.045 in. and a net upward deflection of 0.002 in.

The plate was further loaded to 1600 $\mu\text{in./in.}$ axial strain and 1400 $\mu\text{in./in.}$ circumferential strain. Ten cycles of transverse load were applied yielding the same deflections up and down as before.

Upon loading the plate to 2800 $\mu\text{in./in.}$ equal biaxial strain, 125 cycles of transverse loading were applied. No significant changes in plate strains were observed.

The plate was further loaded to 6400 $\mu\text{in./in.}$ equal biaxial strain. Since no gross buckling or deflection was observed, the plate was unloaded. Permanent set was approximately 4800 $\mu\text{in./in.}$ circumferentially and 5300 $\mu\text{in./in.}$ axially. Maximum vertical deflection was 0.120 in; the average was less than 0.030 in.

Specimen No. 4 contained a 1 in. by 1 in. cooling tube welded full length along the plate centerline in the circumferential direction. Considering the results of the three previous tests, this specimen was loaded from the beginning with a stud missing. The specimen was loaded to 1300 $\mu\text{in./in.}$ in small increments, carefully inspected, and the strain data analyzed, but no significant effects could be found. The plate was therefore unloaded to remove a second stud adjacent to the first missing stud. Upon loading through 1200 $\mu\text{in./in.}$ equal biaxial strain, the plate yielded quite sharply at approximately 1500 $\mu\text{in./in.}$ equal biaxial strain. Again the plate was inspected and the strain data analyzed, but no gross effects could be seen.

The plate was then loaded to produce strain levels of 1700, 2100, 2800, 4000, and 5500 $\mu\text{in./in.}$ equal biaxial strain. As the straining progressed from 1500 $\mu\text{in./in.}$, a severe buckle slowly developed over the two missing studs. At 5500 $\mu\text{in./in.}$ strain, the loading was stopped.

When unloaded, the average permanent set was 1570 $\mu\text{in./in.}$ axially and 1275 $\mu\text{in./in.}$ circumferentially in the center region of the plate. These values are quite low in view of the over-all plate strain, a direct result of the rather large buckle in the panel. The gross upward deflection at the buckle was 0.650 in. Although the cooling tube passed between the missing studs, it was bent sharply by the action of the buckling panel, and its stiffening effect apparently was not significant.

The most significant results obtained from the tests are:

1. In no case did load dropoff occur with increasing strain. Average plate stress versus strain curves are given on Figs. 1.10, 1.11, and 1.12 for specimens 2, 3, and 4, respectively. The results for specimen No. 1 are not given because of the experimental problems encountered.
2. There was no evidence that individual panel buckling precipitates buckling in adjacent panels, which could result in a buckle forming along a considerable length, even with lateral loading.
3. The maximum measured lateral deflections are given in Table 1.1 and typical results are shown on Figs 1.13, 1.14, and 1.15 for specimens 2, 3, and 4, respectively.

Fig. 1.10--Test plate No. 2, stress-strain curves

Fig. 1.11--Test plate No. 3, stress-strain curves

Fig. 1.12--Test plate No. 4, stress-strain curves

Table 1.1

LINER BIAxIAL COMPRESSION TEST LATERAL DEFLECTIONS

Specimen	Biaxial Strain Condition ($\mu\text{in./in.}$)	Max. Deflections in Inches (+ = away from concrete)			Location
		29,000 lb into Concrete	7000 lb away from Concrete	No Lateral Load	
No. 2 flat plate	1700 ^a 1020 ^b	-0.011	+0.023	+0.014	Near center of concrete void
All studs present	2790 ^a 5110 ^b	---	---	+0.047	At edge of concrete void
No. 3, 1/8-in. offset weld	1250 ^a 1360 ^b	+0.008	+0.052	+0.051	At center of concrete void
All studs present	3010 ^a 3000 ^b	+0.059	+0.078	+0.078	At edge of concrete void
No. 4 flat plate, 1 stud removed	1230 ^a 1310 ^b	(c)	(c)	+0.02	At missing stud position
No. 4 flat plate	1510 ^a 1460 ^b	(c)	(c)	+0.045	At missing stud position
2 adjacent studs removed	2840 ^a 2750 ^b	(c)	(c)	+0.305	At missing stud position

^a Circumferential.^b Axial.^c No load cycling during this test.

Fig. 1.13--Test plate No. 2, lateral deflections at center of concrete void

Fig. 1.14--Test plate No. 3, lateral deflections at center of concrete void

Fig. 1.15--Test plate No. 4, lateral deflections at missing stud position

4. The maximum measured local strains are given in Table 1.2. Typical results for back-to-back pairs of gages are given on Figs. 1.16 and 1.17 for specimen No. 3. The average plate strains are given on Fig. 1.18 for comparison.
5. The maximum anchor stud loads measured at approximately twice the predicted strain levels (equal biaxial strain) were, for specimen No. 3, 8,620 lb at 3,000 $\mu\text{in./in.}$ and for specimen No. 4, 11,360 lb at 2,800 $\mu\text{in./in.}$
6. The specimens withstood at least twice the number of vessel depressurization lateral load cycles without either fatigue cracks or increase in displacements.

The maximum predicted liner strains are:

1. Maximum equal biaxial strain, 1050 $\mu\text{in./in.}$ in the barrel section near the haunch with the vessel depressurized.
2. Maximum unequal biaxial strain at the vessel midheight, depressurized: (1) hoop strain, 1500 $\mu\text{in./in.}$, (2) meridional strain, 760 $\mu\text{in./in.}$

From the results of the liner biaxial compression tests, it is concluded that the liner and anchor studs will withstand all potential loading conditions without developing deformations that could adversely affect the performance of the liner, anchors, thermal barrier, cooling tubes, or internal equipment. The effects of twice the maximum calculated loadings on the liner in the presence of either isolated ineffective anchor studs or the maximum permissible weld offset are acceptable. This includes the effect of a pressure of 125 psi behind the liner due to a cooling tube leak with the vessel depressurized. The results obtained are considered to be conservative, since the yield stress of the material tested was 47,000 psi compared with the minimum value specified for the liner material of 56,000 psi. With the higher yield stress material, onset of buckling would be delayed.

Prestressing System

The relaxation test procedure on the 25-wire stress-relieved straight tendons was changed to include heating two tendons to 120°F and leaving the other two tendons at ambient temperature. The two tendons were heated after completing about 1000 hr after retensioning the tendons to 0.7 GUTS. Elevated temperature relaxation data over a period of 1000 hr have been obtained and are being evaluated. The purpose of the change was to obtain tendon loss information for a sequence of events (tensioning, retensioning, and heating) which simulates the probable sequence of events relative to the use of stress-relieved wire tendons in the PCRIV.

The first tendon specimens were stabilized (thermalized) wires manufactured by Richard Johnson and Nephew, Ltd. of England were received. These were two 25-wire straight tendons and one 168-wire tendon for a 10-ft-radius curved tendon layout. The two 25-wire straight tendons are being tested for relaxation data, one at 120°F and the other at ambient temperature. In order to accomplish these relaxation tests on the stabilized-wire tendons, two relaxation tests on stress-relieved wire tendons were terminated and the tendons

Table 1.2
LINER BIAXIAL COMPRESSION TEST STRAIN GAGE DATA

Specimen	Biaxial Compressive Strain Condition (μ in./in.)	Local Strain (μ in./in.) ^a (+ = tensile)			Location
		29,000 lb into Concrete	7000 lb away from Concrete	No Lateral Load	
No. 2 flat plate	1700 ^b 1020 ^c	-3420	-2350	-2520	Gage on top surface of plate near center of concrete void
All studs present	2790 ^b 5110 ^c	---	---	-8500	
No. 3, 1/8-in. off- set weld	1250 ^b 1360 ^c	-1820	-2960	-2920	Gage on lower surface of plate near center of concrete void
All studs present	3010 ^b 3000 ^c	-7250	-8200	-8190	
No. 4 flat plate, 1 stud removed	1230 ^b 1310 ^c	(d)	(d)	-2200	Maximum strains measured; gages on top surface of plate near edge of buckle
No. 4 flat plate	1510 ^b 1460 ^c	(d)	(d)	-2800	
2 adjacent studs removed	2840 ^b 2750 ^c	(d)	(d)	-4950	

^a Data given for gage giving largest strain range.

^b Circumferential.

^c Axial.

^d No load cycling during this test.

Fig. 1.16--Test plate No. 3, circumferential strain gage pair near center of concrete void

Fig. 1.17--Test plate No. 3, axial strain gage pair near center of concrete void

Fig. 1.18--Test plate No. 3, average plate strain

replaced in the test rig. Relaxation information will be obtained for at least one year on these stabilized-wire tendons.

The full-size (168-wire) stabilized-wire tendon has been prepared for testing in the 10-ft-radius curved tendon layout. The tests will include in sequence, (1) friction, (2) cyclic load, and (3) ultimate failure tests. The cyclic load test will involve the application of at least 1000 cycles between 1275 and 1525 kips (0.7 GUTS $\pm$ 15,000 psi) with the tendon anchored at 1400 kips. The cyclic load test is for the purpose of determining the effect of load cycling on the ultimate strength and elongation characteristics of the stabilized-wire curved tendon.

Additional stabilized-wire tendon specimens will be delivered for relaxation tests on the curved tendon layouts. Other tests being conducted on the stabilized wires include fatigue testing of individual wires.

Concrete Properties

Two instrumented 7-in. concrete cube specimens were cast and sealed; the specimens are under water cure at 74°F prior to testing for triaxial creep. The hydrostatic pressure chamber has been functionally checked.

Sixteen sealed concrete cylinders were placed under the load and temperature conditions given in Table 1.3. These creep specimens will remain under load and data will be obtained for approximately six months. Sixteen sealed cylinder specimens scheduled for loading at age 200 days are still under water cure at 74°F.

Table 1.3
LOADING CONDITIONS FOR CREEP TESTS

No. of Specimens	Age at Load (days)	Temp. (°F)	Stress Level (psi)
2	98	74	2250
2	98	74	2820
2	104	74	3390
2	104	74	3960
2	98	150	2250
2	98	150	2820
2	104	150	3390
2	104	150	3960

Approximately 150 days of data have been accumulated on the 30-day-old cylinder specimens that were placed under loads of 2250 and 2820 psi and at temperatures of 74° and 150°F. The creep strains still exhibit linearity on log-time basis, and the creep strain rates observed are not significantly different from those reported in the previous quarterly report (GA-8270, Table 1.10). The creep tests on the 30-day-old cylinder specimens are continuing.

Biaxial creep tests on sealed cruciform specimens were completed. The test procedure was changed to include variations in the load levels applied to the cruciform specimens in order to obtain information for the confirmation of the viscoelastic analytical procedure. Sample data obtained from two cruciform tests are given on Figs. 1.19 and 1.20.

The loading (stress and temperature) history of the cruciform specimens are given on Figs. 1.19 and 1.20. Both specimens were cured at 74°F for 30 days after casting. Before loading, the specimens were heated to 150°F. The thermal expansion strains measured during the temperature rise are not shown on Figs. 1.19 and 1.20. The cruciform specimens were loaded at 32 days of age to a stress level of $\sigma_H = \sigma_V = 1585$ psi, which corresponds to 0.3 f'_c . This stress condition was maintained on both specimens for 63 days, after which some variations in stresses were introduced. Specimen 20622 + B052 (Fig. 1.19) was completely unloaded in the vertical direction ($\sigma_V = 0$) while the horizontal load ($\sigma_H = 792$ psi) was reduced by half. Specimen 20622 + B047 (Fig. 1.20) was subjected to half the original stresses ($\sigma_H = \sigma_V = 792$ psi) in both directions. This second load condition was maintained for 30 days, after which both cruciform specimens were reloaded to their original stress condition of $\sigma_H = \sigma_V = 1585$ psi. After another 20 days, both specimens were unloaded completely. "Creep recovery" measurements at 150°F are continuing for approximately 60 days, after which the specimens will be cooled to 74°F and monitored for another 60 days. Similar load variation experiments were conducted on cruciform specimens at 74°F.

Data obtained from the cruciform tests have not been fully evaluated; however, preliminary results and observations pertinent to Figs. 1.19 and 1.20 are presented below.

Creep and shrinkage rates obtained during the 63-day period when the specimens were under $\sigma_V = \sigma_H = 1585$ psi stress and 150°F temperature are given in Table 1.4 for the five-gage locations shown on Figs 1.19 and 1.20. The rates given represent the slope of the line from a least-squares fit of the data using a strain/log-of-time relationship.

Table 1.4
CREEP AND SHRINKAGE RATES

Specimen (Region)	Gage Symbol	Rate ($\mu\text{in.}/\text{in.} - \log_{10}$ Time)	
		20622 + B052	20622 + B047
Uniaxial (vert.)	L2	275.5	315.4
Biaxial (vert.)	L3	216.5	218.8
Biaxial (horiz.)	W3	163.9	226.0
Uniaxial (lat.)	T2	125.2	64.7
Biaxial (lat.)	T3	70.4	54.0

One of the objectives of the biaxial creep tests was to determine the variation of Poisson's ratio with time, using the lateral strain measurements (T2 and T3). As shown on Figs. 1.19 and 1.20 and given in Table 1.14, the

Fig. 1.19--Test data from cruciform specimen 20622 + B052

Fig. 1.20--Test data from cruciform specimen 20622 + B047

lateral strain variation with time is in the compressive direction, although initial tensile strains were recorded at load application. This observed behavior is attributed to the large influence of shrinkage at the early age and elevated temperature, which could very well obscure any Poisson effect. From Figs. 1.19 and 1.20, significant shrinkage is indicated within 40 to 50 days (concrete age approximately 70 to 80 days from casting date) after heating, and practically no increase is observed after that.

The effect of complete and partial unloading is illustrated on Figs. 1.19 and 1.20. From the uniaxial gage (L2) in Fig. 1.19, creep recovery appears complete after about 30 days from unloading, and the magnitude of strain is approximately 50 μ in./in. Upon partial unloading by 50%, the uniaxial and biaxial strains (L2, L3, and W3) on Fig. 1.20 indicate significant reduction in creep rates, due not only to reduction in stress but also to the aging effect of concrete.

When reloaded to their original stress condition of $\sigma_H = \sigma_V = 1585$ psi, the principal strains exhibited relatively rapid creep within the first 10 days (delayed elastic response) as shown on Figs. 1.19 and 1.20. It appears that if the virgin curves obtained during the 63-day creep test were extrapolated, the creep curves would coincide with the reloading curves after the delayed elastic response has been completed.

Information presented on Figs. 1.19 and 1.20, together with other results of the load variation tests on cruciform specimens, will be utilized for confirming the viscoelastic analysis procedure that will be made on the particular cruciform geometry and for duplicating the stress and temperature history used in the tests. Of particular interest is the verification of the superposition principle used in the analysis to account for stress variation effects on concrete creep.

THERMAL BARRIER

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

Friction and Wear

Friction and wear screening tests on candidate Class B thermal barrier materials for service at approximately 1500°F in dry helium, were completed. Of the fifteen material combinations evaluated (see Fig. 1.21), two were selected as having optimum characteristics. These are flame-sprayed chromium carbide (item 4) and a Co-Cr-W alloy, Stellite Star-J metal (item 7).

Fig. 1.21--Summary of Class B screening tests

The testing of potential thermal barrier materials utilizes friction and wear test equipment described in detail in an earlier quarterly report (GA-8038). In general, the button-plate friction and wear specimens shown in Fig. 1.22 were tested in oscillatory sliding mode at 800°C (1472°F), in pure helium for a total sliding distance of 2400 in. The bearing load between mating surfaces was 100 psi. The performance of a given button-plate material combination was analyzed on the basis of (1) coefficient of friction measurements made during test, (2) assessment of relative surface damage after test, (3) cost, and (4) ease of application.

The frictional forces were monitored during each test and recorded at intervals of 100 cycles; i.e., 50 in. of sliding. Figure 1.23 shows the frictional behavior of various flame-sprayed coatings under identical test conditions. It should be noted that the chromium carbide coating has a relatively low coefficient of friction. The chromium oxide coating is lower, and it would have been preferred to the chromium carbide coating if the magnitude of the coefficient of friction were the only basis for selection. Unfortunately, heating from room temperature to the 800°C test temperature and subsequent cooling to room temperature invariably resulted in the complete spalling of the chromium oxide coating. The chromium oxide is therefore not recommended for service at 800°C. Since experience has shown that erratic readings generally indicate a progressive deterioration of the mating surfaces, such readings are interpreted as an unfavorable indication of service performance. Referring again to Fig. 1.23, the chromium carbide coating maintained a relatively low and constant coefficient of friction throughout the test, with only a slight scatter in data. A relatively low and constant coefficient of friction during test was also experienced with Stellite Star-J metal. The tungsten carbide coatings, on the other hand, showed a slow increase in this coefficient of friction with time.

Since a major consideration in the performance of mating thermal barrier surfaces is their ability to form tight seals to prevent helium flow, then any phenomenon that would prevent intimate contact could also lead to degradation in thermal performance. Hence, adequate performance of bearing surfaces implies that gross welding, distortion, grooving, etc., should not occur. Both chromium carbide and Stellite Star-J metal performed well in this regard. No evidence of adhesive wear was observed visually or microscopically. In fact, a slight polishing or burnishing of mating surfaces was apparent, indicating an improvement in the sealing characteristics of the junction.

On the basis of these considerations, the flame-sprayed coatings seem to have considerable promise but their high cost may suggest the use of Stellite rather than flame-sprayed coatings in some areas. Unfortunately, the Stellite material is only available as a casting. This characteristic may cause difficulty in its application to a sheet structure. The chromium carbide coating, on the other hand, is easily applied to any surface.

Thermal Barrier Permeation Test

The rate at which the helium gas primary coolant will permeate and flow within the thermal barrier significantly affects its thermal performance when

Fig. 1.22--"Button-plate" specimen configuration used in friction and wear tests. Top and bottom buttons stationary while plates oscillate between them. Arrow indicates direction of plate oscillation

Fig. 1.23--Flame-sprayed coatings rubbing against themselves

pressure gradients exist along its surface. Analytical predictions of the heat transfer resulting from this flow show that a rate of around 2 lb/hr through a thermal barrier panel 24 in. wide by 72 in. long is acceptable. The greatest surface pressure gradient found along the length (72 in.) of any thermal barrier panel under reactor operating conditions is 0.50 psi. The pressure difference felt by the thermal barrier panel itself is reduced by the cover plates and seals, which partially seal the thermal barrier panel, and result in a pressure difference of only 0.016 to 0.03 psi being felt by the thermal barrier panel, depending on the internal flow resistance of the thermal barrier panel.

A series of tests has been conducted in which essentially full-scale samples of multilayer metal thermal barrier have been subjected to conditions simulating the pressure differences across their edge seals. These seals are placed around the edge of thermal barrier panels between individual layers, in order to limit the permeation flow through that panel. Two designs have been tested: one is a solid round wire (Figs. 1.24 and 1.25) and the other is a flexible sheet-metal channel (Fig. 1.26). Each type of seal is compressed between the thermal barrier sheets by a row of bolts around the edge of the panel and just inside the seal.

Each panel was tested by putting it in an autoclave in which the pressure of the gas surrounding the panel could be raised above ambient. In the center of the test panel was a vent tube that passed through the wall of the autoclave to sensitive flow meters and that was ultimately exhausted to the atmosphere. All tests were conducted using dry nitrogen, and the results were corrected to account for the difference in gas properties at reactor conditions. A radiant heater opposite the hot face of the panel and cooling tubes on the liner next to the cold face were capable of producing a realistic temperature change from 750° to 130°F across the thickness of the panel. This method of testing causes radial flow into the panel from the entire periphery rather than a flow in at one end and out the other as in the reactor. However, the conditions experienced by any element of the seal are realistic, and the flow values obtained for such an element may be readily correlated with the over-all flow in a reactor panel.

The results of these tests have been reduced and correlated with reactor flow conditions for the thermal barrier panels mounted on the liner in the location of the high surface pressure gradient. Table 1.5 shows the sheet-metal-channel seal design to be clearly superior to the round-wire seal design for resisting permeation flow, and this design has therefore been adopted as the reference design.

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.

Fig. 1.24---Thermal barrier panels with round seal wires between sheets

Fig. 1.25--Round seal wires in edge of thermal barrier panel

Fig. 1.26--Thermal barrier panel for permeation flow test (with channel edge seals)

Table 1.5
RESULTS OF THERMAL BARRIER PERMEATION TEST

	<u>Round Wire Seal</u>	<u>Sheet-metal- channel Seal</u>
Pressure difference along 72 in. length:		
In gas stream, psi	0.5	0.5
Across thermal barrier panel, psi	0.016	0.03
Helium flow resulting in panel:		
At room temperature, lb/hr	17.4	2.9
At 750°F, lb/hr	19.2	2.3
At R.T. after 40,000 thermal cycles, lb/hr	21.8	---

The tests on the second Region I flow model are directed principally at three areas:

1. Gas velocity profiles into the steam generators, especially under conditions of nonuniform gas flow from the reactor core.
2. The core exit temperature probe environment.
3. Temperature mixing of the gas between core and steam generator modules.

In the previous quarterly report (GA-8270), the model design was described and the Item 1 tests discussed. The effects of these test data on the performance of the steam generator modules have since been evaluated, and no problems are foreseen due to the velocity profiles produced in the modules even with the worst core exit flow distributions.

Test and analysis for Item 2 are discussed below. Tests and analysis for Item 3 are continuing.

Core Exit Temperature Probe Environment

One of the core exit regions of the model was designed to simulate the individual flow channels from each of the seven fuel columns supported by the core support block. The temperature sensing probe of this region was simulated by a wooden cylinder having 38 thermocouple elements distributed over its surface, which corresponds to the outer surface of the graphite sleeve containing the temperature probe in the reactor. The thermocouples were recessed and smoothed to present a smooth cylindrical surface to the air flow. The location of the probe relative to the fuel column gas streams and the location of the thermocouple elements are shown in Figs. 1.27 through 1.30.

Heated air was introduced to each of the seven fuel columns in turn, and to groups of two and three columns while ambient air was being drawn through the remaining columns. In each case, the hot air flow was regulated

Fig. 1.27--Results of temperature sensing probe test; 162°F air supplied to column 4

Fig. 1.28--Results of temperature sensing probe test; 160°F air supplied to column 1

CORE REGION NO. 22

Fig. 1.29--Results of temperature sensing probe test; 221°F air supplied to column 1

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Fig. 1.30--Results of temperature sensing probe test; 160°F air supplied to column 2

to equalize the dynamic head in all fuel columns. The temperature of the hot stream was varied to determine the dependence of the flow pattern on the differential temperatures of the streams. The location of the specimen core exit region was also varied to determine the dependence of the flow pattern on core exit region location.

Temperatures on the surface of the sensing probe were measured at each thermocouple location. Typical results of the tests are shown in Figs. 1.27 through 1.30 with isothermal lines added to indicate the region of influence of each test stream. It is seen that the influence of each stream on the surface temperature of the probe is relatively local and independent of core region location and stream differential temperatures.

These data were used as input to an analysis that assumed that the probe was a graphite tube containing temperature sensing elements. The analysis showed that this provides satisfactory averaging of the fuel column exit temperatures. For example, with variations of column gas exit temperature of 140°F, a sensor at the radial and axial center of the tube would read region mixed mean gas temperature to a maximum error of $\pm 15^\circ\text{F}$.

Other Test Results

Test data are still being analyzed, but the following preliminary comments may be made:

1. Pressure losses in the primary helium coolant system. The pressure losses measured on the model when converted to prototype conditions are all reasonably close to those predicted. Over-all measured pressure loss from the core support block to the steam generator flange, when converted to prototype design conditions, is about 1.0 psi compared with the estimated loss of 1.16 psi.
2. Back pressure of the central fuel column passages. The back pressure of the central fuel column passages in the core support block is only slightly higher than that in the peripheral fuel column passages. The differences are negligible when compared with the flow resistance in the fuel columns in the core and may be neglected.
3. Frequency of vortex shedding from sensors and posts. Investigation of the vortex shedding phenomena from the temperature sensing probes in the core support blocks and from the core support posts has so far shown little evidence of coherent vortex trails from these components. More tests are planned with a revised probe traversing device.
4. Static pressure on core support floor. The static pressure on the upper surface of the core support floor was measured at eight locations. Converted to prototype design conditions, the pressures are shown in Fig. 1.31. These data are being used in the design of the core support floor thermal barrier.

Fig. 1.31--Plenum floor static pressure distribution converted to prototype design conditions

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.

The purified helium compressor components have been fabricated and assembled by Mechanical Technology, Inc. (MTI). The compressor has been run free in air at 24,000 rpm with a number of starts and stops. Operation was smooth and appeared to be satisfactory.

The test loop has been fabricated and received by MTI.

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

The thermal cycling tests of two Geminol and two Chromel-Alumel thermocouples in the test facility described in the previous quarterly report (GA-8270) is continuing. Simulation of 180 shutdown cycles and 150 scram cycles was completed during the previous quarter, and the third stage of thermal cycling to simulate power changes is now under way. The hot junction is heated to 1650°F and then cooled to 1400°F on a 60-min cycle. Approximately 3000 cycles have been completed.

Moisture Detectors

Difficulties with low signal levels due to light attenuation in the fiber optics system have been eliminated by the use of a photocell with better spectral matching and the use of a preamplifier. Both the reflected and the scattered light channels now operate several orders of magnitude above the noise level.

The electronic circuitry for all dewpoint devices will be identical. A switch will permit the following modes of operation:

1. Trip mode with automatic switchover to indicating mode.
2. Trip mode.
3. Indicating mode only.

The circuit includes the following features:

1. Trip will occur when reflected light decreases 5% or more per minute as compared to the output of 10 min earlier. This feature permits a close trip setting without false trip should there be slow deterioration of the optical system.
2. The 10-min delay circuit is automatically charged up when the equipment is turned on.
3. Trip will occur when reflected light deteriorates slowly to 30% of initial output.
4. Solid-state switchover from trip to indicating mode upon moisture trip is employed.
5. Reset of trip can only be performed when the mirror is dry, i.e., holdover of the reset button does not eliminate trip.
6. All circuits are being mounted in NIM type modules, recommended by the AEC Committee on Nuclear Instruments.

Step increases of moisture were applied to the detector in the trip mode. The trip settings were 27°F (100 ppm by volume at 700 psia) or above. The increased moisture levels exceeded the trip setting by a margin of 25% or more. The instrument tripped in 1 sec or less.

In the indicating mode, the response time to a step increase in moisture is as follows:

<u>Step Increase In Moisture (at 700 psia)</u>	<u>Response (sec)</u>
-87° to -65°F (0.1 to 0.5 ppm)	90
-65° to +27°F (0.5 to 100 ppm)	120
+27° to 128°F (100 to 3000 ppm)	120

The mirror temperature rises by heater action during the above operating conditions when there is a moisture rise. When it is necessary to bring the mirror down in temperature, the mirror is cooled with liquid nitrogen. The detector takes longer to be cooled than to be heated. For example, taking the mirror temperature from 27° down to -87°F (100 to 0.1 ppm at 700 psia) requires a total of almost 10 min.

For the detector operated in the trip mode, a large sample flow is required to obtain fast response. For the detector operated at the low end of the indicating mode, -87°F (0.1 ppm at 700 psia), a small flow is required to limit the heat load on the mirror. An attempt has been made to find a compromise flow value to eliminate sample flow control. It has been determined that a sample flow of 15 std cm³/sec is acceptable for the entire range of the instrument.

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.

Planning is under way for the test program on the auxiliary equipment and special tools. The first phase of the program consists of the developmental cooling test for the reactor viewing device and the developmental vacuum test for the reserve shutdown vacuum tool. The developmental cooling test for the reactor viewing device will consist of evaluating the pressure drops, flow rates, and heat transfer peculiar to the long flexible metal hose used to carry the coolant gas. The problem of cooling the reactor viewing device camera for in-core viewing is similar to the cooling problem encountered by Pye Ltd. of Cambridge, England, when they built cameras for in-core viewing of the gas-cooled magnox type reactors. The developmental test on the reserve shutdown vacuum tool will be primarily aimed at developing the vacuum capabilities of the vacuum power head through a 25-ft-long probe while in hot helium.

The second phase of the test program consists of checkout and proof tests of all the auxiliary equipment and special tools in conjunction with the fuel handling machine tests.

The core service manipulator is a unique design necessary to fill specific needs. Figure 1.32 shows the manipulator attached to the fuel handling machine. The manipulator has five discrete motions and uses three motions from the fuel handling machine. All motions are derived from electric motors located inside the mechanism.

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.

The intent of the present phase of the test program is to establish the rupture characteristics of various types of rupture disks. The purpose of the disks is to retain absorber material in the reserve shutdown storage hoppers until conditions require its release into the core and the operator over-pressurizes the hopper to rupture the disk.

Various configurations of metallic disks were tried without success. In general, the disks ruptured at design pressure, but failed to open out to an acceptable size of aperture (i.e., approximately 3-3/4 in. diam). Figures 1.33 and 1.34 depict the conditions before and after rupture of a typical metallic disk.

Rupture tests with flat, circular graphite disks gave very promising results. Rupture pressure was consistent and the disks disintegrated into fragments approximately 5/8 in.² in area.

Fig. 1.32--Core service manipulator

Fig. 1.33--Typical metal disk before rupture

Fig. 1.34--Typical metal disk after rupture

Tests were performed on two different grades of graphite (HLM and ATJ) of three different thicknesses (0.156, 0.205, 0.25 in.) at room temperature. There was no significant difference in their fragmentation characteristics. ATJ Grade is a molded, high-density graphite, and HLM is an extruded, medium-density material.

Further tests are under way at operating temperature conditions (800°F) using the same materials and thicknesses to obtain comparative data for the material at elevated temperatures. To date these test results appear very promising.

When an optimum rupture pressure and material grade has been selected as a result of the foregoing tests, it is planned to make a series of drop tests with the hopper loaded with granular absorber material. These tests will show whether a granular type of material has any tendency to agglomerate or interlock in such a manner as to retard or prevent its free fall from the hopper into the core. The design of the system is such as to permit the use of spherical material up to 11/16 in. in diameter should the need arise.

Some comparatively minor but time-consuming problems were encountered in connection with making an effective seal between the hopper and the disk holder on a repetitive basis. The problem was solved by the introduction of Inconel O-rings at the mating surfaces and seal welds. The leakage problem did serve to emphasize the need for a reasonably leak-tight system in order to avoid the need for excessive quantities of gas to rupture a disk.

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.

Testing with the full-scale orifice vibration and flow test rig has been completed, and reduction and analysis of the data are essentially complete. A total of 402 test runs were made, of which 194 were conducted specifically to study vibration characteristics, and the remainder to gather a combination of flow and additional vibration data. The following is a summary of conclusions drawn from analysis of the data.

Test data indicate that the design of the flow distribution plenum beneath the orifice provides for adequate distribution to all columns within the region. No significant pressure gradients existed within the plenum to cause maldistribution of flow among the seven fuel columns within the region. Values of coolant hole mass flow rates were found consistent throughout the region within approximately 5% to 7% of the average value, which is considered within the accuracy of the instrumentation systems.

The fundamental natural frequency of the orifice shutter in the torsional mode was found to be approximately 2 cps. This motion is heavily damped, presumably due to bearing friction, since oscillations cease after about two cycles following a step release from an initially displaced position. Under flow conditions, this torsional oscillation did not occur.

Visicorder traces of the torque transducer signal, which would be very sensitive to torsional oscillations, are very stable under all flow conditions. It appears from the data that there are no flow-induced torsional oscillations to interfere with the performance of the orifice as a flow regulating device.

The movable outer shell was observed to experience a "ringing bell" type of vibration at about 550 and 2000 cps of 10G peak-to-peak amplitude in the radial direction at the bottom of the skirt and about 4G peak to peak at the same frequencies in the radial direction at the top of the skirt (see Fig. 1.35). These small amplitude vibrations appear typical of maximum values to be expected under reactor operating conditions.

The movable outer drum experienced a flow-induced torque in the closing direction. The magnitude of the torque reached a maximum of about 300 lb-in. under the conditions tested and was found to be a function of position angle and pressure drop. Extrapolating these data to reactor conditions results in an estimated maximum operating torque of approximately 400 lb-in. at a position angle of about 15° to 30° from wide open. Modification of the design has been initiated based on these data.

CORE SUPPORT SYSTEM

The objective of this subtask is to develop a core support system that meets the mechanical requirement of adequately supporting the reactor core under all conditions of reactor operation, and under credible accident conditions.

Tests on graphite materials being considered for the core support structure including mechanical and physical property tests and friction and wear tests were completed during the quarter. The tests performed and the materials included in the mechanical and physical property tests are given in Table 1.6. Five specimens of each grade were tested. Tensile, compression, and flexural tests were carried out at room temperature; thermal conductivity and thermal expansion tests were carried out at temperatures ranging from 22 to 800°C. The materials tested in the friction and wear studies are given in Table 1.7. Helium used to simulate HTGR coolant was atomic grade, purity 99.995%. The moisture content of the exhaust helium was maintained below 10 ppm at 2 to 5 psig to simulate reactor conditions. Experiments were conducted with a moisture content of about 2 ppm. Thirty-year life travel distances were simulated in the tests at operating temperatures of room temperature, 400, and 800°C. Data from these tests are now being reduced and evaluated.

Tests which simulated the movements due to thermal cycling were conducted on a full-size core support block and post assembly this quarter. The block was cycled over the movements representative of those expected for various reactor operating conditions. The test was performed in a dry helium environment, the block and posts being loaded to design conditions.

Fig. 1.35--PSC orifice shutter vibration measurements. All readings are peak-to-peak values to approximately 1 G accuracy. The quadrant noted was scanned at the bottom of the skirt with a moveable accelerometer (radial) at 15° intervals. All recordings in this quadrant were 10 G's peak to peak

Table 1.6

TESTS PERFORMED ON CANDIDATE MATERIALS FOR CORE-SUPPORT COMPONENTS

Core-support Component	Candidate Grade	Raw Stock Dimensions (in.)	Test Parameter Measured ^a						
			UTS	UCS	MOR	E	CTE	K	ρ
Blocks	HLM ^b	42D by 23L	-	-	-	-	-	-	-
	MHLM		X	X	X	X	X	-	X
	PGX		X	X	X	X	X	-	X
Posts	H249	6D by 42L	X	X	X	X	X	-	X
	H359		X	-	X	X	X	-	X
	H271 ^b		-	-	-	-	-	-	-
	ATJ ^c		X	X	X	X	X	-	X
Seats and Inserts	H205	7-1/2D by 8L	X	X	X	X	X	X	X
	ATJ ^d	8-1/2D by 4L	X	X	X	X	X	X	X
Base	B205	7-1/2D by 6L	X	X	X	X	X	X	X
	TS574		-	-	-	-	-	-	-
	TS835		X	X	-	X	X	X	X
	CBN		X	X	-	X	X	X	X

^a-Tests performed are indicated by "X" for each grade; UTS = ultimate tensile strength, UCS = ultimate compressive strength, MOR = modulus of rupture, E = modulus of elasticity, CTE = coefficient of thermal expansion, K = thermal conductivity, ρ = density.

^b-Initially selected grade for evaluation, replaced subsequently by others from same manufacturer. H271 replaced by H249 and H359; H249 eliminated in favor of H359 on basis of preliminary tensile screening tests.

^c-ATJ round-molded bar stock, approximately 9 in. by 12 in.

^d-ATJ rectangular-molded bar stock, 8 in. by 18 in. by 50 in.

Table 1.7

FRICTION AND WEAR STUDY ON CANDIDATE MATERIALS FOR CORE SUPPORT-COMPONENTS

Core Support Design Simulation	Friction and Wear Couple Material Designation	Method of Graphite or Carbon Forming
Block material on block material	PGX versus PGX graphite MHLM versus MHLM graphite	Molded Molded
Post material on seat material	ATJ versus ATJ graphite H359 versus H205 graphite	Molded Extruded
Base material on liner material	TS835 (18 wt-% MoS ₂) graphite versus A-537 grade B steel (CHARPAC) B205 baked carbon versus A-537 grade B steel (CHARPAC)	Unknown: extrusion or molding direction de- termined by thermal expansion measurements Extruded

The cyclic tests, which were carried out in the rig shown in Fig. 1.36, included:

1. 55 cycles 1 in. total movement of the block; 1/2 in. relative movement of the keys in the keyways.
2. 360 cycles 7/8 in. total movement of the block; 7/16 in. relative movement of the keys in the keyway.
3. 40,000 cycles 3/8 in. total movement of the block; 3/16 in. relative movement of the keys in the keyway.

In conducting the tests the block was cycled one half the required number of cycles along a line through two opposite corners of the block and then along a line bisecting two opposite flats for the remainder of the required cycles.

Results of the tests showed no significant wear or degradation of the contacting surfaces, and no significant change in the freedom of movement of the assembly.

A test of a 3/4-scale model of the graphite core support block and post assembly to failure under vertical load was completed. The assembly was initially tested with three posts supporting the block and with the block offset horizontally 3/4 in. from true center. A load five times the design load was applied with no significant effect on the graphite components.

A second test was then performed on the same block with one post removed (Fig. 1.37). A load factor of five was again applied with no complete failure of components occurring. However, at a load of slightly over three times design load, there was a change in the load-versus-deflection curve. Upon examination of the components at the conclusion of this test, cracks in the graphite block were found near the location of the removed post and several hairline cracks were observed at the key and keyway locations at the bottom side of the block on the sides opposite the removed post.

A final test to failure was then performed after replacing the third post. A load approximately eight times the design load was required to cause failure of the block. Failure was initiated at the previously removed post location, and the failure mode was similar to that experienced in the previous test reported in an earlier quarterly report (GA-7634). The cracked block is shown in Fig. 1.38.

Fig. 1.36--Core support structure cyclical test rig (containment removed)

Fig. 1.37--Core support structure failure test (one post removed)

Fig. 1.38--Core support block failure

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.

Alternative Fuel Cycles

The reference Fort St. Vrain fuel cycle is characterized by a six-year fuel life, C/Th-225 in the equilibrium cycle, and a yearly refueling of one-sixth of the core. As reported in the previous quarterly report (GA-8270), this cycle is nearly optimum when flux peaking, rod worth, and fuel cycle cost are all considered. In particular, if the fuel lifetime were reduced to four years, an increase in fuel cycle cost of about 0.2 mill/kw-hr would result. Recent studies have extended this survey to a three-year fuel lifetime, with the result shown on Table 2.1. These data are consistent with those displayed on Fig. 2.1 in the previous quarterly report. An increase in fuel cycle cost of 0.3 to 0.4 mill/kw-hr would attend a three-year reduction in the fuel lifetime. This penalty would obviously be much reduced if only a few of the regions were refueled at three-year intervals. A three-year lifetime for the six five-column refueling regions, for example, would cost about 0.04 to 0.05 mill/kw-hr, a differential that would diminish as future fabrication costs were reduced.

Effect of Loading Tolerances on Power Distribution of Initial Core

The tolerances on the fuel and poison loads, thorium load, graphite density, etc. will lead to some flux tilting in the initial core; the magnitude of the effect is shown on Table 2.2. The flux tilting due to loading tolerances is generally much less than the uncertainties in calculating the actual three-dimensional flux distribution. Nevertheless, these data have been used to evaluate fabrication processes and are the result of some iteration between design and reasonable fabrication requirements.

Flux tilting due to loading tolerances in subsequent cycles will be less than those shown in Table 2.2, because only one-sixth of the core will be refueled each year. Also, it is expected that the actual loadings will be well within the currently specified tolerances.

Statistical methods have been used to determine the relationship between the loading tolerance and flux tilt (Ref. 1). Using successive numbers from a table of normal random numbers, the deviation from nominal in the uranium

Table 2.1
FUEL CYCLE CHARACTERISTICS
(Three Year Fuel Lifetime, Nonrecycle^a)

C/Th Ratio	Fuel Lifetime	Conversion Ratio	Specific Power	Age Peaking Factor ^b	MWD/t	Total Annual Fissile Movement		Relative Fuel Cycle Cost
						Charge (kg)	Discharge (kg)	
200	3	0.68	1320	1.16	44,000	280	180	0.48
	6	0.63	1160	1.43	87,000	205	85	0.06
225	3	0.66	1520	1.20	49,000	260	150	0.40
	6	0.61	1310	1.55	97,000	200	70	0.01
250	3	0.64	1710	1.25	54,000	250	130	0.35
	6	0.59	1460	1.67	107,000	195	60	-0.01
275	3	0.62	1870	1.30	60,000	240	110	0.32
	6	0.57	1570	1.77	115,000	195	55	-0.01

^aData consistent with those shown on Fig. 2.1 of previous quarterly report (GA-8270).

^bAge peaking factor is ratio of fuel density in fresh fuel to core average fuel density; it is a measure of minimum gross radial peaking factor.

Table 2.2
FLUX PEAKING FACTORS RESULTING FROM TOLERANCES IN INITIAL CORE LOADING

	Tolerance (%)	Uncertainty in Flux, (%)	
		Gross Radial Flux	Local Flux Peak
U loading, local 1-in. length of fuel rod	±10	nil	±10
Th loading, local 1-in. length of fuel rod	±10	nil	±3
U loading, refuel. region	±5 ^a	---	---
Th loading, refuel. region	±6 ^a	±2.4	±2.4
Graphite loading in refuel. region	±1 ^b	±1.6	±1.6
Graphite imp. in ppm of equiv. B	±1 ppm	±3.0	±3.0

^aA 5σ limit.

^bA 3σ limit.

and thorium loadings for each of 37 refueling regions was determined. It had been previously determined that the $\pm 1\%$ tolerance should be treated as a 5 σ limit. This gives an acceptably low probability that any one refueling region, even in a 1000-Mw(e) HTGR, exceeds the $\pm 1\%$ tolerance. A series of flux tilting coefficients were then determined. These relate the flux change in each of the core refueling regions to a small change in the metal loading of any single region. When combined with the loading deviations previously determined, a modified power distribution can be calculated and the maximum change in flux can be noted.

If this process is repeated several hundred times (the flux tilting coefficients remain the same, of course), the frequency with which a certain maximum flux change occurs can be determined. The largest of these maxima is the number of interest, and it has been recorded in Table 2.2.

Cross Sections for Nuclear Analyses

In preparation for the fifth iteration in the nuclear design, to be designated Reference Design 5, a new set of cross-section libraries has been developed. These reflect new basic cross-section data for many nuclides, notably U^{233} and U^{235} , as well as an improved method for estimating resonance region cross sections. A review of the fuel element geometry and composition for the initial core of Reference Design 4 led to the following conclusions with respect to the resonance region cross sections:

1. Resonance region absorption cross sections are very insensitive to the C/U ratios. The outscatter cross section is more sensitive to the C/U ratio; if the C/U ratio increases, as it does with depletion, the outscatter cross section also increases.
2. If the thorium density in the rod is held constant, the resonance region absorption cross sections are also insensitive to changes in the C/Th ratio.
3. The thorium cross section is relatively sensitive to the Dancoff factor, all other parameters remaining constant; other cross sections are relatively insensitive to the Dancoff factor.
4. The thorium cross section at constant C/Th ratio is quite sensitive to the thorium concentration in the rod, whereas the other cross sections are insensitive to this parameter. The same is true with respect to cross-section sensitivity to the rod diameter.

It was concluded that the principal parameters governing the epithermal cross sections are the thorium density in the rod and the hole diameter (and therefore Dancoff factor) for the given lattice pitch. The less important parameters are the C/U and C/Th ratios and the scattering cross section in the rod. This approach toward specifying cross sections complements the recently adopted approach toward specifying fuel hole diameters, which is described later.

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.

Nuclear Design of High Thorium Zone

In the HTGR there tends to be a large flux spike at core-reflector interfaces. At the top interfaces, the power spike does not usually result in excessive temperatures, because the average power density in the absence of a spike tends to be low. The situation is further helped by the low existent temperatures and the presence of the control rods. The power spike at the bottom interface can result in high temperatures, however, and axial zoning is sometimes necessary.

Radial zoning and a cold reflector combine to give a high power spike at the core - radial reflector interface. This is reduced by putting a heavy thorium load into the four rows of fuel holes immediately adjacent to the outer reflector. While not yet complete, the nuclear design is proceeding along the following lines:

1. Fuel hole diameter: ~0.547 in.
2. Thorium and uranium zoning: axial, two; azimuthal, none.
3. C/Th ratio in zone: initial, ~125; reload, ~150.

The treatment of the burnable poison is still unsettled, but the poison pins located in the high thorium zone will probably be the same as in the rest of the element.

Fuel Hole Diameters

The fuel rod is composed of BISO-coated* fissile and fertile particles, charcoal, and a carbonaceous binder. The charcoal and binder form the matrix that gives the fuel rod its form and strength. To minimize power peaking at the ends of the fuel rods and also the centerline fuel temperatures along the length of the rod, it is important that the particles fill the fuel hole uniformly and completely. It had originally been planned to size each hole so that it would be filled with a specified load of coated particles. Recently, a tentative decision was made to have, at most, four different hole diameter sizes. The minimum hole diameter in a given refueling region would be large enough to hold the reference loads of uranium and thorium in reference coatings. Lower uranium and thorium loadings would be obtained by a suitable increase in the coating thickness or particle diameter, or perhaps in the charcoal or binder loads.

The advantages of this approach are:

1. The much smaller number of different fuel hole and fuel rod sizes

*Late in the period covered by this report, the reference fuel was changed to incorporate SiC in the coatings. This will be covered in the next quarterly report.

will decrease fabrication costs by simplifying drilling, fuel rod molding, and quality control.

2. The nuclear design will be simplified.
3. Most particles will have a somewhat thicker coating than they would otherwise have had, thereby making them stronger.

There are at this time no evident disadvantages of consequence in this approach. The variation in particle diameters necessary to assure full fuel holes falls easily within the allowable spread in particle diameters. The change in the core graphite load due to the use of slightly oversized holes is very small. The average density of carbon in the hole is about 1.4 g/cm^3 , while the average density of bulk moderator graphite is about 1.7 g/cm^3 , the differential being applicable over about 1% of the core volume. Moreover, the nuclear design and fuel cycle costs are primary functions only of the C/Th and C/U ratios and not the graphite loading itself.

Tentative fuel hole diameters are 35/64, 32/64, and 30/64 in. Fuel rod diameters will be nominally 0.009 in. smaller than the fuel hole diameter. For a constant kernel diameter, the adjustment in the coating thickness of either particle necessary to obtain a full fuel hole in the worst case is about 50 μ , or if both coatings are adjusted, about 30 μ .

Power Distribution During a Change in Power Level

During a change in power level the average core temperature will change. As reported in the quarterly report for the period ending June 30, 1967 (GA-8038), the resulting compensatory rod motion will alter the core power distribution and, thereby, the heat balance between steam generator modules. The degree of imbalance is a function of the reactivity involved and the number and location of the rod pairs used during the maneuver. The imbalance has been found to be larger than desirable in most cases. While it is possible at any given time to find a rod program that if used would keep the modules balanced, it appears impractical to specify such a program. This has led to the examination of alternative methods of maintaining steam generator balance, with automatic feedwater flow adjustment appearing most attractive at this time.

The "reference" rod program for the 100% to 25% power change is shown on Fig. 2.1. In various refueling regions, power density change of $\pm 10\%$ are common. Feedwater control will be designed to compensate for such changes.

Axial Zoning

The reference design has been based on two axial fuel zones. Recent studies indicate some advantages for an unzoned core.

The average axial power distribution of the current two-zone reference design was discussed in the quarterly report for the period ending March 31, 1967 (GA-7039). The significant influence of the axial temperature distribution and the axial graphite gaps was pointed out for this case, which is

EQUILIBRIUM CYCLE
 ROD PAIRS 8,10,12,14,16,18 (PARTIALLY INSERTED)
 $\Delta K = 0.0116$

MIDDLE OF INITIAL CYCLE
 ROD PAIRS 8,10,12,14,16,18 (PARTIALLY INSERTED)
 $\Delta K = 0.0010$

Fig. 2.1--Percent change in power distribution during a 100% to 25% load change

reproduced on Fig. 2.2. This does not yield an optimum temperature distribution, however; centerline fuel temperatures are about 200°F higher at the core midplane than at the bottom of the core. An improved distribution of fuel and poison yields the modified curve shown on Fig. 2.2, for which the centerline fuel temperature is also shown. The temperature distribution is applicable for a fuel rod of average power density in which axial conduction is taken into account. The fuel temperature at the midplane and at the bottom core-reflector interface are nearly the same, about 1830°F.

The reference two-zone scheme has half of the fuel elements in each zone. In an alternative scheme, also shown on Fig. 2.2, one-third of the elements are in the top zone and two-thirds in the bottom zone. The average temperature in the top half of the core is higher with the 1/3-2/3 scheme than with the 1/2-1/2 scheme. In a fresh core, an optimum 1/2-2/3 design might lead to peak fuel temperatures that are, perhaps, 50°F less than an optimum 1/2-1/2 design, but depletion effects could reduce this advantage.

It has been estimated that the peak centerline fuel temperature of a newly loaded core with several axial fuel zones would, at best, be not lower than 1700°F for an average element, but probably between 1700° and 1750°F. Hence, a two-zone design yields temperatures not far from the best obtainable, and the effects of control rods and depletion could reduce the differential even further.

The power distribution and temperatures for an axially unzoned core are shown in Fig. 2.3. Peak fuel temperatures for a fresh fuel element of average power density are about 50°F higher than for the reference two-zone case. If the relative power density of the fuel element is 1.83 instead of 1.0, the centerline fuel temperature at the core midplane is essentially the same as at the bottom of the core. For this "hot" element, zoning would make things worse.

Time-dependent control rod and depletion effects will now be studied, and if the results with an unzoned core are acceptable, zoning will be re-examined.

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.

Axial Xenon Stability

One consideration in fixing the core height at about 15.5 ft was the problem of xenon stability. The high core temperature of the HTGR would make the detection of axial oscillations difficult, and their control would require an undesirable use of control poison. Recent studies have shown that the core is very stable against axial oscillations; pertinent results

Fig. 2.2--Reference axial two-zone distributions

Fig. 2.3--Unzoned axial distribution

are shown on Fig. 2.4 for the axially zoned core. In the bottom curve, it is shown that the perturbed power density at a point about 25% down from the top of the core rapidly approaches the initial steady-state value with an essentially negligible overshoot. On the basis of this analysis, which is in agreement with analytical studies as well as with other more general calculational results, it has been concluded that axial xenon oscillations are not possible and that provisions for detecting and controlling them are not necessary.

Time Constants for Heat Removal from the Core

After an instantaneous power pulse strong enough to raise the fuel temperature by several hundred degrees, the following response was observed:

1. Heat transfer between coated particles takes much less than 1 sec.
2. Within 4.5 to 5.5 sec, the temperature of the fuel decreases to 50% of its peak value.
3. The moderator temperature peaks in about 14 sec.
4. The outlet gas temperature peaks in about 80 sec.
5. Depending on pulse size, it takes 100 to 130 sec to remove half of the inserted energy from the reactor.

During the transient, the flow rate and inlet gas temperature were assumed to remain constant.

The long time constants for heat flow in the reactor are due to the large heat capacity of the fuel element. As reported in the quarterly report for the period ending June 30, 1967 (GA-8038), the average core heat capacity is about 35 Btu/ft³-°F. If the full core power is stored in the core, its temperature will rise only about 5°F/sec.

Rod-pair Withdrawal Accident at Source Power

An extended and improved analysis of the source power rod-pair withdrawal accident (RWA) has been completed, superceding the analysis reported in the quarterly report for the period ending September 30, 1966 (GA-7453). An RWA in the equilibrium cycle was shown to be more severe than in the initial cycle, but even with peaking factors of 6, system temperatures were not excessive during the accident. The period scram at low power levels is effective in limiting the consequences of this accident. Calculations bearing on these points and others are described below.

In the previous quarterly report (GA-8270), it was shown that the maximum rod-pair worth in a critical core appeared to be no higher than 0.038 Δk in the initial cycle and 0.033 Δk in the equilibrium cycle. Although a very unusual combination of events was necessary for this situation to occur, these rod worths were used in this analysis.

A peaking factor of 6 has been assumed for the hottest refueling region. Such a value obtains if a critical condition has been reached with a normal rod configuration.

Fig. 2.4--Axial xenon stability

A scram reactivity of $0.06 \Delta k$ has been used in most of these analyses. The minimum nominal reactivity available for scram is $0.063 \Delta k$, occurring at midcycle at $300^\circ K$ in the initial core. In the equilibrium cycle, the nominal scram reactivity is about 50% larger.

The rate of reactivity addition during a scram is shown on Fig. 2.5. Curve 1 is applicable to the scram of one rod pair in an otherwise unrodded core. Curve 2 is applicable to the insertion of one rod pair in an otherwise fully rodded core. Curve 3 is applicable to the bank movement of 37 rods. Curve 4 is applicable to the insertion of six rod pairs (e.g., in ring 2) in an otherwise unrodded core. The curve applicable to the source power RWA is similar to curves 1, 2, or 4. Curve 1 has been assumed in the most recent analyses, although the effect of assuming other curves has been studied.

During an RWA, reactivity is added at a rate that is also given by the curves on Fig. 2.5, specifically Curve 1 in these analyses. The time scale, the abscissa, must be reversed, however.

Unless otherwise noted, the assumptions in Table 2.3 were also used in the calculations.

Table 2.3
OTHER ASSUMPTIONS USED IN RWA ANALYSES

Rod-pair withdrawal time, sec	180
Scram time, sec	149
Scram delay time, sec	0.2
Inlet gas temperature, $^\circ F$	400
Time dependency	Constant
Flow rate, % rated	10
Time dependency	Constant
Initial fuel and mod. temp., $^\circ F$	400
	<u>Initial</u> <u>Equilibrium</u>
	<u>Cycle</u> <u>Cycle</u>
β_{eff}	0.0050 0.0046
Λ , 10^{-4} sec (neutron lifetime)	1.9 2.6
Temperature Coefficient, $10^{-5}/^\circ C$:	
300 $^\circ K$	-11.0 -8.7
600 $^\circ K$	-7.0 -4.9
900 $^\circ K$	-3.9 -2.4
1200 $^\circ K$	-2.7 -1.6
1500 $^\circ K$	-2.3 -1.5

The results, given in Table 2.4 and in Figs. 2.6, 2.7, 2.8, and 2.9, show the following:

1. Fuel and gas temperatures are sensitive to the net scram reactivity (reference: Case 1 versus 3 for the initial core and 5 versus 6 for the equilibrium cycle).

Fig. 2.5--Reactivity insertion rates

Table 2.4
SUMMARY OF RESULTS OF ROD WITHDRAWAL ACCIDENT AT SOURCE POWER LEVEL

	PSAR	Case 1	Case 2	Case 3	Case 4	Case 5	Case 6	Case 7	Case 8
Cycle	Equil.	Initial	Initial	Initial	Initial	Equil.	Equil.	Equil.	Equil.
Rod Worth, Δk	0.033	0.038	0.038	0.038	0.038	0.033	0.033	0.033	0.033
Scram reactivity, Δk	0.060	0.060	0.060	0.030	0.060	0.06	0.10	0.06	0.03
Scram time, sec	149	149	149	149	3.6	149	149	149	149
Scram rate curve No.	1	1	3	1	3	3	3	1	1
Scram signal, sec:	140% pow.	140% pow.	140% pow.	140 or 5% ^a	140% pow.	140% pow.	140% pow.	5 ^b	5 ^b
Time of signal	~30	~30	~30	~30	~30	~30	~30	~17	~17
Time rods move	~30	~30	~30	~30	~30	~30	~30	~23	~23
Peak power, Mw(t)	2060	1700	1700	1700	1225	2060	2060	1100	1600
Rel. power density of hottest region	6.0	6.0	---	---	---	---	---	---	---
Max. rise in avg. fuel temp, °C	120	110	290	200	25	340	180	80	170
Max. rise in max. fuel temp, °C	~825	825	---	---	---	---	---	---	---
Max. rise in avg. outlet gas temp, °F	160	150	~425	~300	~20	---	---	---	270
Max. rise in hot region outlet gas temp, °F	~730	730	---	---	---	---	---	---	---
Ref. fig.	2.7 or 2.8	2.6	2.6	2.7	2.6	2.8	2.8	2.9	2.9

^aSystem temperatures are essentially independent of whether a scram occurs at 140% or 5% rated power.

^bDecades per minute.

Fig. 2.6--Rod-pair withdrawal accident from source power (initial cycle)

Fig. 2.7--Rod withdrawal accident from source power (initial and equilibrium cycles)

Fig. 2.8--Rod withdrawal accident from source power (equilibrium cycle)

Fig. 2.9--Rod withdrawal accident from source power (period scram, equilibrium cycle)

2. Core temperatures are sensitive to the rate with which scram reactivity is added (reference: Case 1 versus 2 for the initial core and PSAR versus Case 5 for the equilibrium cycle).
3. A fast scram mechanism is very effective in limiting core temperatures (reference: Case 2 versus 4).
4. The scram on a fast period is also very effective in limiting core temperatures (reference: PSAR versus Cases 7 or 8).
5. System temperatures are insensitive to whether a scram occurs at 140% or 5% rated power (reference: Case 3).

Some important conclusions from this work are:

1. If the RWA is terminated by a period scram, system temperatures will not be excessive even if the most reactive rod fails to scram. This provides the justification for a period scram in the protective system.
2. System temperatures are sensitive to the rate of scram reactivity insertion. The PSAR analyses are reasonably well based in this regard.
3. Peaking factors much larger than 6.0 could probably be tolerated.
4. A floating high-power-level scram is not a significant advantage in this accident.

Rod Ejection Accident at Full Power

Rod-pair ejection by a refueling penetration failure is considered incredible. Such considerations aside, however, the core temperature excursion in such a hypothetical accident was studied and the results are reported below.

A complete failure of a refueling penetration and all associated restraining mechanisms would lead to an initial acceleration of the rod pair and mechanism of 68 g. The rod pair would be ejected in 0.12 sec, unless, as is likely, the supporting cable failed under the sudden loading, in which case the rod pair could fall back into the core. In addition, both a high-flux and a low-pressure scram signal would be generated.

The fuel temperatures resulting from this accident are shown in Figs. 2.10 and 2.11 under the assumption that the reserve shutdown system was not activated. Other pertinent assumptions and considerations are:

1. Although a depressurization would accompany such an accident, the power level and system temperatures during the first 15 to 20 sec of the accident (the most important period) will be essentially independent of possible assumptions regarding coolant flow and coolant pressure.
2. The accident occurred at beginning of life when the segregation of the fertile material in different particles was a maximum. The effective Doppler coefficient in a "prompt" accident would then be

Fig. 2.10--Rod-pair ejection accident without cable snap

Fig. 2.11-Rod-pair ejection accident with cable snap

the least, since only about 23% of the fertile material in fresh fuel is intimately mixed with the fissile material.

3. The thermal conductivity within the particle was characteristic of that of a fresh particle.
4. The conductivity in the bed was characteristic of that in a loose bed of coated particles.
5. The initial conditions were those of normal full-power operation. A rod worth of 0.015 Δk was assumed. In normal operating rod configurations, the rod-pair ejection accident at source power levels is much less severe than one starting at full power because of the much larger Doppler coefficients.

Figure 2.10 is applicable to the rod ejection without a cable failure. The average temperature of the fissile particle rises by a maximum of 700°C. After 0.5 sec, both fissile and fertile particles are at about the same temperature, 470°C above their initial temperatures. The normal scram begins to be effective in about 10 sec. The maximum rise in the average moderator temperature is 250° to 300°C.

The data in Fig. 2.11 reflect a cable failure. If the rod-pair subsequently falls back into the core, fuel temperatures are high for only about 10 sec, with the fissile and fertile particles rising, on the average, by at most 430° and 380°C. The moderator temperature rise is limited to about 100°C.

If the rod pair does not fall back into the core, particle temperatures rise about 450°C above their initial values for a longer period of time.

If the reserve shutdown system is activated, then regardless of cable failure, the transient fuel temperatures are very close to those obtained with a cable failure assuming that the rod pair falls back into the core (see Fig. 2.11).

Aside from the ejected rod itself, this accident will not significantly affect the structure of the core, and core levitation will not occur. Some fuel particle coatings will fail as a result of the sudden high temperatures. The weakest coatings are on old particles, but these will increase by significantly less than 700°C in temperature. The new particles have the strongest coatings and some of these will rise to above 2000°C. The fuel elements themselves will suffer no damage, and the plant will not lose its ability to shut down or cool down.

Dependence of Control Rod Worth on Diffusion Length

An approximate correlation between the rod worth and the thermal diffusion length and the neutron "age" has been determined; this is shown in Fig. 2.12. This functional dependency of the rod worth [$\Delta\rho$ defined as $(1/k_{\text{rodged}} - 1/k_{\text{unrodged}})$] on L^2 , τ , and k_{∞} is easily shown analytically: $\Delta\rho = a_1 (L^2/k_{\infty}) + a_2 (\tau/k_{\infty})$, where a_1 and a_2 are approximately constants.

Figure 2.12 is based on a calculational model whose composition was characteristic of the average initial loading. Arbitrary variations in the fuel, poison, and thorium loads were made to obtain specific data points. The plot was then used to predict the rod worth in the equilibrium cycle, a value of $\Delta\rho = 0.138$. An explicit calculation gave $\Delta\rho = 0.132$. Although the correlation is only approximately correct, some useful inferences can be drawn.

Suppose an increase in the parasitic capture cross section occurs predominantly in the thermal-neutron energy group. Both L^2 and k_∞ will decrease by about the same amount. Hence, the change in $\Delta\rho$ will be due to the τ/k_∞ term, and the rod worth as measured by $\Delta\rho$ will increase. If an increase in the fission cross section occurs, however, then both L^2/k_∞ and τ/k_∞ change in the same direction, and the rod worth will decrease. The rod worth thus appears to be quite sensitive to the uranium load.

An increase in the fast parasitic capture rate, due perhaps to an increase in the thorium load, will cause an increase in $\Delta\rho$ due to the increase in the L^2/k_∞ term.

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.

Heat Generation in the Control Rods

Heat generation in the control rods is due to (n,α) reactions in the B^{10} and gamma ray energy deposition in both the poison body and the steel structural members. The (n,α) energy deposition is very spatially dependent. The gamma ray energy is deposited fairly uniformly over the control rod, however, in the following amounts for a region of average power density: 0.63 w/cm³ of absorber material, 3.2 w/cm³ of steel.

In a rodded refueling region with an average power density equal to the core average power density, the (n,α) energy deposition averages 3.8 w/cm³ of absorber material, or 120 w per linear centimeter of rod length. At the surface of a fresh rod, the value is higher by a factor of about 12; at the inner surface, it is only about half the average value.

For a fully inserted rod, an axial peaking factor of about 1.4 to 1.5 would be applicable. For a partially inserted rod, an axial factor of about 2.0 is applicable at the tip of the rod. There is considerable conservatism in this latter estimate, but it is probably adequate for estimating peak temperatures and temperature gradients in the rod.

Fig. 2.12--Dependence of rod worth on L^2 , τ , and k_{∞}

The relative power density of a rodded refueling region is usually about 0.7 to 0.8, but power densities as high as 1.1 have been computed.

Fuel Element Exposure

The current design points for fuel element exposure are:

Maximum fissions per initial metal atom:

Fissile particle, 0.20

Fertile particle, 0.07

Fast-flux exposure ($E > 0.18$ Mev) for fully irradiated fuel:

Average, 5.5×10^{21}

Maximum, 8.5×10^{21}

The fraction of the discharged fuel that experiences a given exposure is shown in Fig. 2.13. Only in the case of those fuel elements in the initial core that remain in the core seven years are exposure limits exceeded by a small amount (about 1%) of the fuel. These data are being used to assess fuel element performance.

References

1. Stevens, D. W., A Statistical Analysis of the Effects of Regional Fuel Loading Tolerances on Flux Tilting in the Fort St. Vrain HTGR, USAEC report GA-8115, Gulf General Atomic, to be published.

Fig. 2.13--Fuel element exposure

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.

At the end of this reporting period a decision was made to include a layer of SiC within the pyrocarbon coating of both the fissile and fertile particles for the reference design fuel elements.

Mechanical Design

The fast-neutron flux decreases by approximately a factor of two between the center and the outside edge of fuel elements adjacent to the side reflector. This spatial gradient of fast-neutron exposure causes the fuel elements in the outermost ring of fuel columns to contract nonuniformly, which leads to horizontal gaps between stacked fuel elements. These gaps provide paths for coolant flow between neighboring coolant holes, and between the coolant holes and vertical clearance spaces between the elements.

The computer code CONCAL was used to calculate the contraction of the graphite in the outer ring of fuel elements. The model of the fuel element used by CONCAL was exactly the same as a RAT-3D heat transfer model of the fuel element so RAT-3D-calculated temperatures could be used directly in CONCAL. To estimate the distribution of contraction, the fuel elements are divided into smaller parts and the neutron-induced contraction of each of the smaller parts is calculated independently of the other parts that make up the element.

The local contraction at any material point is a function of the history of the neutron flux and temperature of the material point. Time variations in the fast-neutron fluxes were taken from two-dimensional GAMBLE calculations and radial variations in fast flux were taken from one-dimensional FIRE calculations in the core region adjacent to the side reflector. The temperature distributions used in CONCAL were calculated by the RAT-3D model of the fuel element. Flows used in the RAT-3D model were from a detailed FLOPSY model representing a portion of one PSC fuel column. Temperature distributions from RAT-3D calculated at reactor residence times for elements of 1 year, 3 years, and 6 years were used in CONCAL. Linear interpolation of temperature versus time was used for times between the calculated values. The computer code used these neutron fluxes and temperatures to find fuel

element graphite contraction by interpolation on a table of experimental graphite contraction previously stored in the computer.

Table 3.1 shows the net axial contraction differences between the inside and outside of fuel elements located in the outermost ring of fuel columns and the resulting gaps which form between elements within a column. Horizontal gaps calculated by CONCAL are compared in Table 3.1 with those used in the FLOPSY crossflow network code.

Thermal Design

Calculations continued to evaluate the thermal performance of the core. FLOPSY, a code describing a connected network of flow paths, was used to determine the flows in coolant channels, in horizontal gaps between fuel elements, and in vertical gaps between columns of fuel elements for various types of orificing. The flows calculated by the FLOPSY code were used (1) with a RAT-3D heat-transfer model incorporating crossflows to calculate fuel and graphite temperatures and (2) with a heat-balance code OUTTEM to estimate region average helium outlet temperatures.

Crossflow Analysis. The analytical model describing crossflow was extended from three seven-column fuel regions to seven regions of seven columns arranged to describe a portion of the core. In developing the analytical model the following assumptions were made:

1. The bypass flow gaps, defined as the voids between fuel element columns, were calculated on the basis of nominal component dimensions corrected for thermal expansion and graphite contraction due to irradiation. The bypass flow gaps are therefore a function of age of the fuel elements and are shown in Table 3.2.
2. Fuel elements adjacent to the side reflector are exposed to a large radial gradient in fast-neutron flux causing corresponding nonuniform contractions in the graphite. The result is that a gap is opened at the interfaces between fuel elements within each column. The magnitude of the interface gap was estimated to be a maximum of 0.25 in. at the end of six years and facing the core axial centerline. On the side facing the reflector, the interface gap was assumed to be 0.002 in., which was determined experimentally for new fuel elements resting on top of one another. The loss coefficient parameter (K/A^2) for crossflow between the coolant holes and the bypass channel was determined experimentally for various interface gaps and for various edge distances between the coolant hole and the bypass channel. The loss coefficient parameter is defined by the equation

$$\Delta P = \frac{K}{A^2} \cdot \frac{\dot{m}^2}{2\rho} \quad , \quad (1)$$

where ΔP = pressure difference between the coolant hole stream and the bypass channel

Table 3.1

COMPARISON OF HORIZONTAL GAPS USED FOR FLOPSY ANALYSES AND GAPS
CALCULATED USING TEMPERATURE DISTRIBUTIONS BASED ON FLOPSY RESULTS

Element	Three Years			Six Years		
	Contraction (in.)	Gap (CONCAL) (in.)	Gap Used in FLOPSY	Contraction (in.)	Gap (CONCAL) (in.)	Gap Used in FLOPSY
1 (top)	0.07	0.035	0.031	0.14	0.070	0.062
2	0.10	0.085	0.062	0.19	0.165	0.124
3	0.10	0.10	0.125	0.22	0.205	0.250
4	0.075	0.087	0.125	0.19	0.205	0.250
5	0.082	0.078	0.125	0.165	0.178	0.250
6	0.057	0.070	0.062	0.090	0.128	0.124
		0.028	0.031		0.045	0.062

Table 3.2

VERTICAL GAP WIDTH BETWEEN COLUMNS AND REGIONS

Fuel Element Description	Age of Region (years)						Ext. ^a Gap (in.)	Bypass Gap Area for 7 Regions (in. ²)
	1	2	3	4	5	6		
Top Reflector	0.078	0.096	0.109	0.117	0.124	0.130	0.207	167
Axial Location:								
1	0.095	0.128	0.152	0.168	0.180	0.191	0.224	221
2	0.091	0.125	0.150	0.166	0.169	0.190	0.220	216
3	0.069	0.090	0.106	0.116	0.124	0.131	0.196	163
4	0.056	0.069	0.079	0.085	0.091	0.095	0.176	131
5	0.053	0.066	0.076	0.082	0.087	0.091	0.173	127
6	0.050	0.062	0.071	0.076	0.081	0.085	0.166	121
Bottom Reflector	0.044	0.052	0.057	0.061	0.064	0.067	0.159	105

^aThe external bypass gap is defined by the boundaries of each fuel region and is open to the top plenum.

$\dot{m}$ = crossflow rate

ρ = fluid density

Values of crossflow loss coefficient parameter used in the analysis are shown in Fig. 3.1. As indicated in the figure, the crossflow loss coefficient for interface gaps greater than 0.06 in. had to be extrapolated from the available test data. The method of extrapolation was based on satisfying continuity at each interface gap.

3. The coolant temperature of the core region outlets is calculated with some error because the heat gained by the coolant flow in the bypass gaps is not known. In the calculation, it was assumed that the temperature of the bypass flow is equal to the mean coolant temperature at each axial plane, and that the effective reduction in the heat gained by flow in the primary coolant holes is proportional to the power produced in the particular fuel region.

It was calculated that crossflows will be sufficiently large into old regions adjacent to the side reflector that helium outlet temperatures from these regions could exceed 1470°F with fully closed core orifice valves in those old regions and with correctly adjusted valves in adjacent regions. It is therefore necessary, in order to produce uniform helium inlet temperature to all of the twelve steam generator modules, to compensate by overadjustment of the flow through the neighboring regions. This scheme of controlling region outlet coolant temperatures to produce equal steam generator inlet temperatures is possible for maximum orifice valve coefficients ($K_{\max}$) greater than 260. The orifice valve coefficient is defined as the number of velocity heads of pressure loss, based on the top reflector coolant channel velocity, which are taken across the valve.

As regions grow older and power generation decreases, the orifice valve will be progressively closed by the operator to keep the region outlet coolant temperature as near the reactor nominal value as possible. The largest gaps for crossflow have been formed in old regions adjacent to the side reflector. In an old fuel region adjacent to the side reflector with its orifice valve closed, most of the coolant flow out of the region through the support blocks will have entered the region as crossflow. The possibility arises that fuel in the top of an old region is insufficiently cooled while coolant entering from crossflow lower down has resulted in cooler than nominal region outlet helium temperature. To prevent overheating upper fuel elements, a minimum "leak" flow area in the valve is necessary. Leak flows lead to a lower value of orifice valve $K_{\max}$. The RAT-3D code was used, with FLOPSY-calculated flows, to determine the highest acceptable value of $K_{\max}$ which would not result in overheating of fuel in the top of old regions. The maximum allowable value of $K_{\max}$ was found to be about 1000. It was concluded from these analyses that the acceptable range of closed orifice valve coefficients is between 260 and 1000.

Fuel Element Temperature Analysis. Analysis of fuel element temperature distributions under crossflow conditions continued this quarter. Calculations were made using the RAT-3D computer code, a three-dimensional heat-transfer code able to treat internal cooling surfaces. The section analyzed is shown in Fig. 3.2. The model extends the full length of the active core

Fig. 3.1--Crossflow loss coefficient parameter K/A^2 versus fuel element interface gap

Fig. 3.2--Horizontal cross-section of fuel element showing RAT-3D model for crossflow fuel element thermal calculations

(187.32 in.), and is bounded by lines of symmetry within the fuel element. The model includes the following features: (1) the power generation rate may be varied independently within each fuel hole, thereby allowing inclusion of the effects of edge power peaking, (2) the mass flow rate in each coolant hole may be step-changed when passing through a fuel element interface into the adjacent downstream element, which will account for the effects of cross-flow at fuel element interfaces, and (3) the outer surface is specified as a coolant channel, thereby accounting for the effects of heat dissipation by gap flow. The following approximations were made in formulating the analytical model:

1. The effect of side-reflector power peaking was assumed to be a local perturbation superimposed on local reference average region power such that the integral of the peaking is equal to 1.0 across the fuel elements adjacent to the side reflector. This distribution is shown in Fig. 3.3.
2. Due to computer storage limitations, the model contains only one axial node point per fuel element. The two-zone axial power distribution curve shown by Fig. 3.4 was integrated over each of the six fuel elements to obtain an average axial power factor to be used in connection with the single node point per fuel element. As can be seen from inspection of Fig. 3.4, these average values coincide very closely with the curve at the fuel element midpoints in all but the top and bottom elements, thus affording the assurance that the average node point temperatures calculated in these elements are representative of the exact solution at the midpoints.
3. In calculating the bypass gap coolant temperature, it was assumed that both walls are at the temperature calculated for the wall contained within the boundaries of the model. Computer storage limitations precluded extension of the model into adjacent elements. The magnitude of the local bypass gap flow rate is apportioned in accordance with wetted perimeter ratios in each axial segment.
4. Crossflow from an external region bypass gap into a coolant hole at fuel element interfaces is assumed to enter the coolant hole at the local temperature calculated for the external gap flow at that axial station. It is then assumed to mix completely with the flow in the channel.
5. Crossflow from internal region bypass gaps into coolant holes is treated in a similar manner, with the exception that if crossflow occurs from the external gap to the internal gap, it is assumed to enter the internal gap at the local temperature calculated for external gap flow at that axial station and then mix completely with the internal gap flow. The resulting mixed mean temperature is then assigned to crossflow from the internal gap into coolant holes.
6. The Nusselt number correlations used are shown in Fig. 3.5. Laminar values were used up to a Reynolds number of 7500, and turbulent correlations above 15,000. Values in the transition region were obtained by linear extrapolation on a log-log plot as shown in Fig. 3.5. The gap flows are treated as flow between infinite parallel plates with the approximation that the hydraulic diameter is equal to two times the local gap width.

Fig. 3.3--Power peaking factor at side reflector-core interface

Fig. 3.4--Axial power distribution used in crossflow thermal analysis

Fig. 3.5--Nusselt number correlations for RAT-3D thermal crossflow model

Fuel element temperature distributions were calculated for two extreme regions while varying the upper limit value of the loss coefficient for a fully closed orifice. One was the maximum-powered refueling region (region 24, $P/\bar{P} = 1.83$) due to its high relative power coupled with side reflector power peaking; the second was the minimum-powered, six-year-old five-column region (region 35, $P/\bar{P} = 0.483$). The latter is of special interest because of the combination of reflector edge peaking, the fact that the region contains only five fuel columns, and the relatively large crossflow gaps (approximately 0.25 in.) due to age and the fast-flux gradient in proximity to the core reflector interface. It was of concern that in attempting to maintain near-nominal exit temperatures from this region (by throttling down the orifice to counterbalance the effect of coolant inflow at interfaces), temperatures in the upper fuel elements may exceed acceptable limits. This effect, in conjunction with the lower flow fraction required by only five columns, governs the selection of limiting dimensions for the various leakage paths through a fully closed orifice valve.

A typical five-column region was also analyzed for conditions after three years of operation (region 26, $P/\bar{P} = 1.21$). The dimensions of the crossflow gaps in this region at the end of 3 years (approximately 0.12 in. maximum) is about 1/2 the value predicted after 6 years.

The analyses were repeated for each of three values for the fully closed orifice loss coefficient $K_{\max}$: 130, 300, and 1000. Higher values of $K_{\max}$ correspond to lower leakage flow rates.

From the results, the following conclusions were drawn concerning the effects of crossflow on fuel element temperatures:

1. In newer high-powered regions, crossflow at fuel element interfaces is small. However, the total region flow rate is less than that ideally assigned because the core Δp is reduced by the excessive inleakage of coolant into older regions. The result is a modest increase in fuel element temperatures with the maximum fuel center-line temperature in region 24 increasing approximately 100° to 200°F above ideal. (Ideal refers to the results of prior analyses in which crossflow was assumed not to exist.) The results of the analysis for region 24 are shown in Fig. 3.6, in which it is clearly indicated that decreasing values of $K_{\max}$ (meaning greater inleakage of coolant into old regions) is accompanied by increasing fuel element temperatures in the higher-power regions. The values shown in Fig. 3.6 do not include the highly localized thermal effects of power peaking due to unfueled voids and spacers in the fuel rods at fuel element interfaces. This effect has been assessed in prior analyses to be approximately 150°F in region 24.
2. The effects in older lower-powered regions were examined, and the limiting case is refueling region 35. As greater degrees of throttling are employed with the orifice for this region in an attempt to raise exit gas temperature up to nominal conditions, fuel element temperatures in the upper elements increase above nominal values in direct proportion to $K_{\max}$, while temperatures in the lower half of the core, including the coolant, remain below nominal. This

Fig. 3.6--Estimated effects of crossflow on fuel centerline temperature maximum power ($P/\bar{P}_{core} = 1.83$) one-year-old region

is due to the large crossflow inleakage at fuel element interfaces. As shown in Fig. 3.7, the fuel centerline temperature reaches about 2150°F near the midpoint of the second fuel element for a $K_{\max}$ of 1000. Figure 3.8 is a crossplot of region 35 fuel centerline temperatures at axial station $Z = 0.25$ as a function of $K_{\max}$, and assuming that the orifice valve is in the fully closed position.

3. Figure 3.9 shows the results for a five-column region at the end of three years. In this case, even though a definite crossflow gap does exist (about 0.12 in.) at fuel element interfaces, the temperature distributions are responding in a manner similar to new refueling regions having little or no crossflow gap. The axial distributions remain similar to ideal with fuel centerline temperatures ranging up to about 125°F above ideal, with a maximum value of approximately 2050°F at core midplane, neglecting the effect of unfueled lengths in the fuel rods.
4. Coolant leaving the exterior region bypass gaps was calculated to be approximately 50° to 150°F lower (depending on variations in local power generation and crossflow effects) than coolant leaving fuel elements within the refueling regions.
5. Coolant in interior region bypass gaps was calculated to exit between 0° and 150°F higher than coolant exiting surrounding fuel elements. The higher values were found in the newer regions in which the general effect is for interior gaps to supply flow to the exterior gap as the core is traversed axially. This, in turn, allows the interior gap coolant to more nearly approach the graphite wall temperature.
6. Exit coolant temperature from the two most extreme core regions is shown in Fig. 3.10, for various values of maximum orifice coefficient.

Steam Generator Inlet Temperature. The coolant temperature imbalance at a steam generator module inlet was estimated using source-sink relations (S_{ij}) which are defined as the fraction of flow originating at source i which is distributed to sink j . Preliminary values for the source-sink relations were obtained from experimental data on the Region 1 flow model tests. The temperature imbalance at a particular steam generator module is then defined as

$$T_j = \frac{1}{\dot{m}_j} \sum_{i=1}^{37} S_{ij} \dot{m}_i T_i \quad , \quad (2)$$

where $\dot{m}$ = flow rate, T = temperature, and

$$\sum_{j=1}^{12} S_{ij} = 1 \quad . \quad (3)$$

Fig. 3.7--Calculated effects of crossflow on fuel centerline temperature; five-column region at end of 6 yr

Fig. 3.8--Estimated maximum fuel centerline temperature; five-column region at end of 6 yr

Fig. 3.9--Estimated effects of crossflow on fuel centerline temperature; five-column region at end of 3 yr

Fig. 3.10--Temperature imbalance for two extreme regions

The ability of the core region flow control orifices to control to uniform coolant temperature at the steam generator modules was determined by investigating two limiting conditions in the core. The limiting conditions are defined as (1) the maximum coolant temperature to module 49 created by closing the orifices of seven core regions that have an appreciable fraction of their flow distributed to the module and (2) the minimum coolant temperature to module 41 created by opening the orifices of seven core regions that distribute an appreciable fraction of flow to the module. Referring to Fig. 3.11, module 41 tends to run above average temperature because of its close proximity to high-powered fuel region 24, while module 49 tends to run below average temperature because of its proximity to lower-powered regions 35 and 36. Figure 3.12 shows the temperature imbalance at the respective steam generator modules for these two limiting cases as a function of effective orifice loss coefficient. As shown, the greater the orifice loss coefficient the greater the effective range of orifice control.

MATERIALS

The objectives of this subtask are to:

1. Determine the characteristics of beds of bonded coated particles by means of fabrication technique investigations, thermal 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 to 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.

Fig. 3.11--Plan view of core outlet region

Fig. 3.12--Limiting module temperature control range effected by core region orifices

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 reference fuel element for the Public Service Reactor consists of an unpurged hexagonal graphite block containing fuel rods (columns of coated fuel particles packed to a high density and bonded together by a carbon matrix). A bond between coated particles is desirable to prevent the particles from pouring out of the element in the event of any damage to the element. However, the fuel rod is not required to be bonded to the graphite container wall. The matrix contains carbonized resin binder and carbon filler. In addition to acting as the bonding agent, the carbon matrix will also serve to sorb metallic fission products that may escape from the coated particles.

Fuel development efforts are directed toward the development of a practical and effective process for preparing bonded bed fuel rods meeting the reactor design and performance requirements. In addition to process development, test and evaluation efforts are being conducted to establish various physical, mechanical, and thermal properties, and environmental compatibility. Irradiation testing is also being done on representative bonded bed samples. Statistical studies of variations in fuel loading are under way.

Fuel Rod Process Development. Under the fuel rod process development program, a fuel rod injection-molding process and a wet-molding process were developed. These processes were thoroughly evaluated and showed promise as a method of fabricating fuel rods as discussed in previous quarterly reports. All development work was done with practice particles that were 200 and 400 μ in diameter with 100- to 140- μ thick laminar pyrocarbon coatings. The particles appeared to remain intact in all specimens fabricated for the development program.

Four types of fissile and fertile coated particles were specified for the first PSC proof test element. These included high-temperature isotropic coatings (HTI), low-temperature isotropic coatings (LTI), and two types of silicon carbide coatings (SiC). The structure of these coatings differs considerably from that of the laminar coatings on the practice particles. When the first proof test element rods were fabricated with the specified

particles by the injection- and wet-molding processes, occasional cracking of the coatings with subsequent hydrolysis of the particles became apparent. This occurred to some degree with all types of particles, and a detailed investigation was initiated to determine the causes and possible solutions.

One of the main tools used in the investigation was a detailed metallographic study of sectioned fuel rods. The objectives were to establish the extent of process breakage of coated particles, the nature and cause of any breakage, the quality of the coating-to-matrix bond, and the general appearance of the matrix. One-inch long carbonized fuel rods (or broken segments of fuel rods) were evacuated and impregnated with an epoxy at 1 atm. After curing the epoxy, samples were sectioned either longitudinally by grinding through the radius or transversely by grinding 1/8-in. into one end of the rod. The sectioned samples were then polished and inspected.

Each particle in the section (300 in transverse sections and 800 in longitudinal sections) was inspected for breakage and the number of breaks was counted. The breakage fraction was estimated, based upon the estimated total number of particles in the section. However, a considerable fraction of the particles had coatings that were obviously damaged by grinding and polishing. These cases were not considered in the count of broken coatings, but were included in the estimate of the total number of particles in the section (because of the difficulty of estimating the number of polishing breaks). Polishing damage occurred in 10% to 30% of the coatings in transverse sections and 40% to 60% in longitudinal sections.

The hydrolysis of the ThC_2 cores caused additional breakage in some samples, but this was minimized by passivating in 50 parts (by volume) concentrated HNO_3 with 50 parts H_2O .

Metallographic examination was particularly useful in identifying damaged coatings, since a significant number of the cracks did not propagate all the way to the outer surface of the coating and were therefore not revealed by hydrolysis or fission-product release tests. Figure 3.13 shows a section of an injection-molded fuel rod containing LTI-coated fissile and fertile particles in a 70% phenolic resin binder - 30% graphite powder filler matrix. The twin crack failures in the two touching particles are typical of most of the particle failures observed, and apparently arise from compressive or bearing stresses.

When the phenolic and furfuryl resin binder materials are carbonized, a weight loss of around 50% occurs. Along with this, the binder coke undergoes substantial shrinkage resulting in an over-all shrinkage of the matrix. A somewhat quantitative measure of this shrinkage was obtained from small cylinders of straight matrix material (0.25 in. in diameter by 0.25 in. long). These samples were cast in a small graphite mold having known dimensions so that the dimensions of the samples could be taken after the curing and carbonizing treatments. After the carbonization cycle, the phenolic resin matrix samples showed a 10% linear shrinkage and the furfuryl alcohol resin matrix showed a 20% linear shrinkage from the original dimensions.

Fig. 3.13--LTI-pyrocarbon-coated fissile and fertile particles in an injection-molded fuel rod with a 70% phenolic resin binder - 30% graphite powder filler matrix. Cracking failure in coatings due to bearing or compressive stresses (200X)

Experiments were conducted to determine whether the shrinkage strains associated with fuel rod curing and carbonization could result in broken fuel particles. Since the shrinkage strains of the fuel rods were already known, tests were undertaken to determine the diametral fracture strain of the fuel particles. The results indicate that all of the fuel particles examined are capable of withstanding approximately four times the strain normally associated with processing shrinkage before failure occurs.

The apparatus used to conduct these tests consisted of an Instron model TM-L precision testing machine, a 0- to 50-lb load cell feeding into a low-level preamplifier that powered an X-Y plotter. Deflections were measured by a precision dial indicator coupled with the testing machine crosshead position gage. The loading apparatus had an over-all accuracy of ± 0.1 lb; the deflection measurements were determined to be accurate to within ± 0.0002 in.

In a typical determination, the coated fuel particle was placed in a shallow depression in the lower platen, which was vibrated lightly to settle the particle. The upper platen was then lowered by hand until a load of approximately 0.2 lb was indicated on the X-Y plotter. This load was then reduced to zero and the dial indicator and machine crosshead position gages positioned at zero. The particle was then loaded using a crosshead velocity of 0.002 in./min until failure occurred. The resulting crosshead deflection was determined by the indicators mentioned previously.

A total of 125 fuel particles were tested. Prior to testing, the particle minimum, maximum, and core diameters were measured by radiographic techniques.

The following assumptions were made in computing the diametral failure strains:

1. The particles are thin-walled hollow spheres.
2. The load was applied across the minimum diameter of the particles.
3. The load-bearing points were 180° apart and aligned parallel to the testing machine axis.

The total diametral strain at failure is then $e = (d_o - d_f)/d_o$, where e is the failure strain, d_o is the original particle diameter, and d_f is the particle diameter at failure. The term $d_f = d_o - S_p$, where S_p is the particle deflection at failure, S_m is the measured deflection, and S_{LC} is the load cell deflection, and $S_p = S_m - S_{LC}$, giving $e = -(S_m - S_{LC})/d_o$, where the negative sign denotes compression.

The load-cell deflection, which is a linear function of load in the range used for these tests, was found to be 0.00004 in./lb. The total load-cell deflection was computed for each particle and subtracted from the measured deflections. The diametral failure strain was then computed for each particle using the minimum particle diameter (maximum e). The load-deflection data obtained by these techniques are shown in Table 3.3.

Table 3.3
LOAD DEFLECTION DATA FOR COATED FUEL PARTICLES

Specimen	Minimum Particle Diameter (10 ⁻³ in.)	Particle Deflection at Failure (10 ⁻³ in.)	Maximum Diametral Strain (in./in.)	Failure Load (lb)
HTI-Fissile XETK-4552	17.06	1.29	0.076	2.82
	16.35	2.44	0.149	1.59
	16.00	1.01	0.063	2.20
	16.00	1.43	0.089	1.79
	14.46	1.04	0.072	1.54
	19.11	1.94	0.102	1.54
	15.84	1.40	0.088	2.49
	16.35	1.39	0.085	2.65
	15.84	----	-----	----
	15.48	1.58	0.102	2.93
Averages:	18.06	1.15	0.920	2.17
HTI-Fertile XTK-4538	24.86	2.67	0.107	8.18
	25.73	2.63	0.102	6.70
	26.56	2.57	0.097	5.73
	27.42	3.02	0.110	9.37
	24.86	2.54	0.002	6.57
	24.03	2.68	0.112	7.89
	30.34	2.92	0.096	9.55
	28.68	2.78	0.097	7.94
	28.68	2.83	0.099	9.28
	27.42	2.72	0.099	6.92
Averages:	26.86	2.74	0.102	7.81
LTI-Fissile XETK-Comp-1	18.12	0.99	0.055	2.78
	17.69	1.29	0.073	2.69
	18.56	0.94	0.051	1.37
	18.56	1.89	0.102	2.71
	19.38	0.99	0.051	2.62
	16.86	1.40	0.083	2.56
	18.99	1.70	0.090	2.76
	20.25	1.68	0.083	3.07
	16.86	1.28	0.076	2.87
	14.97	1.10	0.074	2.49
Averages:	18.02	1.33	0.074	2.59

Table 3.3 (continued)

Specimen	Minimum Particle Diameter (10^{-3} in.)	Particle Deflection at Failure (10^{-3} in.)	Maximum Diametral Strain (in./in.)	Failure Load (lb)
LTI-Fertile XETK-Comp-1	28.25	2.24	0.079	3.97
	38.68	1.61	0.056	4.72
	29.94	1.46	0.049	5.95
	31.20	1.59	0.051	5.18
	30.77	1.60	0.520	4.92
	29.51	1.44	0.049	3.88
	24.47	1.44	0.059	3.88
	28.68	1.43	0.050	4.17
	25.73	1.17	0.045	3.35
	23.21	1.27	0.055	3.35
Averages:	28.04	1.52	0.054	4.34
LTI-Laminar-Fissile XTDX-4373	19.38	1.39	0.072	2.78
	17.30	1.20	0.069	2.56
	17.69	1.01	0.047	2.16
	18.56	0.92	0.049	2.09
	15.17	1.31	0.086	2.18
	15.17	1.11	0.073	2.25
	16.43	0.98	0.060	3.09
	16.04	1.21	0.075	2.27
	15.60	1.09	0.070	2.66
Averages:	16.82	1.14	0.068	2.45
LTI-Laminar-Fertile XTR-4212	23.60	1.32	0.056	7.06
	24.03	1.68	0.070	5.45
	26.56	2.70	0.102	7.45
	23.21	1.32	0.057	4.45
	22.77	1.13	0.050	4.26
	25.29	1.53	0.060	6.68
	25.29	1.18	0.047	5.40
	24.27	0.94	0.039	3.97
	29.08	2.35	0.081	6.33
	25.73	1.28	0.050	5.62
Averages:	24.98	1.54	0.061	5.67

Table 3.3 (continued)

Specimen	Minimum Particle Diameter (10^{-3} in.)	Particle Deflection at Failure (10^{-3} in.)	Maximum Diametral Strain (in./in.)	Failure Load (lb)
XETK-HTI Comp-1	15.44	1.14	0.074	1.60
	18.03	1.40	0.078	2.41
	15.64	1.24	0.079	1.60
	18.24	2.10	0.115	2.64
	16.84	1.28	0.076	1.33
	15.64	1.49	0.095	2.73
	17.04	1.50	0.086	2.41
	14.79	1.53	0.103	1.70
	17.14	1.14	0.066	1.60
	18.59	2.12	0.114	2.10
Averages:	16.74	1.49	0.089	2.01
XTK-LTI Comp-2	27.94	0.74	0.026	6.57
	26.84	1.93	0.072	7.95
	29.69	1.34	0.045	5.55
	29.04	1.51	0.052	6.00
	28.19	1.63	0.058	6.86
	24.54	1.75	0.071	6.48
	24.74	1.44	0.058	5.35
	30.60	2.12	0.069	9.60
	28.99	1.50	0.052	5.63
	25.04	1.47	0.059	5.83
Averages:	27.56	1.54	0.056	6.58
XETK-SiC 4560	16.84	0.83	0.049	2.95
	19.69	1.01	0.051	3.55
	16.00	1.02	0.064	3.20
	13.94	0.94	0.067	4.00
	15.19	0.88	0.054	3.10
	16.84	1.30	0.077	3.70
	17.84	0.82	0.046	3.33
	19.99	1.07	0.063	3.36
	20.54	1.07	0.052	3.30
Averages:	17.43	0.99	0.058	3.40

Table 3.3 (continued)

Specimen	Minimum Particle Diameter (10^{-3} in.)	Particle Deflection at Failure (10^{-3} in.)	Maximum Diametral Strain (in./in.)	Failure Load (lb)
ThC ₂ -PyC (2 ISO coats) 3662-87-N	30.64	2.94	0.096	7.85
	25.99	2.33	0.090	3.10
	26.50	1.55	0.058	3.72
	26.09	1.71	0.066	4.75
	27.09	1.84	0.068	4.05
	25.64	1.81	0.077	3.40
	27.39	2.05	0.075	5.00
	32.19	2.33	0.072	5.61
	27.40	3.16	0.115	4.65
Averages:	27.66	2.19	0.080	4.68
(Th,U)C ₂ ; XETK-LTI, Con. 1 3662-89E	17.69	2.24	0.127	2.75
	17.99	1.61	0.089	3.62
	19.84	3.26	0.164	3.47
	17.49	1.27	0.073	2.08
	18.84	1.93	0.102	3.03
	17.09	1.34	0.078	2.75
	18.09	1.09	0.060	2.70
	20.14	1.64	0.081	3.10
	17.49	2.21	0.126	3.40
	19.70	1.67	0.085	2.06
	17.59	1.81	0.103	4.68
	20.54	2.15	0.105	3.66
	18.29	1.28	0.070	4.20
	20.29	1.27	0.063	3.30
	17.74	1.67	0.094	3.30
	20.64	1.37	0.066	3.16
	17.94	1.63	0.091	2.88
	18.59	1.03	0.055	2.93
	18.24	1.69	0.093	2.70
	19.99	2.63	0.132	3.07
Averages:	18.71	1.74	0.093	3.14

The maximum diametral shrinkage of injection-molded fuel rods resulting from the curing and carbonization process is 0.004 in., resulting in a shrinkage strain of 0.0086 in./in. (0.86%). This value is well below the diametral fracture strains for the coated particles shown in Table 3.3.

The actual conditions prevailing in the bonded fuel rods during curing and carbonization are much more complex than those suggested by the simplifying assumptions made for the data presented in Table 3.3. In a shrinking matrix, the fuel particles are subjected to isostatic forces in addition to the unidirectional forces used in the crush tests. In the isostatic case, the failure strain would be lower than in the unidirectional case, but with much higher failure loads being required. In addition, complex shear forces between particles are probably present, coupled with the possibility that during carbonization matrix cracks could be favorably oriented with highly stressed areas on the particles and thus propagate through the PyC coating. This latter condition could be largely responsible for fractured particles if the matrix-to-PyC bond is stronger for one batch of particles than for the remainder in the fuel rod. Such fractures have been observed at ORNL by Robbins and Hammer (Ref. 1) for propylene-deposited coatings on ThO_2 cores.

The study of the cracking of particle coatings during fuel rod fabrication has resulted in the following tentative conclusions:

1. The matrix material undergoes significant shrinkage during carbonization (10% for the phenolic injection molding resin and 20% for the furfuryl wet molding resin).
2. A strong bond is formed between the matrix and the surface of the outer (isotropic) coating of the particles. There appears to be stronger bonding between the matrix and LTI-coated particles than between the matrix and the HTI particles. This is consistent with the observation that the surface of the LTI coating contains a significant amount of porosity.
3. The coated particles are wet, and a strong bond is formed during the injection and initial curing. When the matrix is subsequently carbonized and undergoes shrinkage while adhering to the particle coatings, significant stresses are set up in the coatings of the close-packed particles.
4. Some of the current particle coatings appear to have significantly less strength than laminar pyrocarbon coatings. It appears that a batch of particles may contain a fraction with weak coatings (1- to 2-lb crushing strength) that will crack when subjected to moderate stress.

All of these conditions appear to give rise to the cracked and broken particle coatings. This may take the form of bearing failures where two particles are in point contact and compressed as shown in Fig. 3.13, or chunks of the coating may be pulled off by the shrinking matrix.

Several different approaches have been tried as solutions to the problem. These include changing the matrix formulations, using overcoating

and coated particles with various pyrolytic carbon coatings, and surface treating and/or overcoating the coated particles with various materials to decrease adhesion of the matrix to the particles. The general results obtained with the three approaches are as follows:

1. Matrix Formulations. Various amounts of charcoal have been added to the standard matrix formulation (70 wt-% resin, 30 wt-% GP-38 graphite filler) in place of some of the graphite filler. These ranged from 7.5 wt-% to all (30%) charcoal. Although most of the formulations showed no improvement, injection-molded specimens from the mix containing 70 wt-% resin, 20 wt-% graphite, and 10 wt-% charcoal appeared somewhat better than those from the standard mix.

Another approach of this type involved the use of an expanding filler material in the matrix to counteract the resin shrinkage. By proper treatment, a bisulfate residue compound can be formed from highly ordered flake graphite. This compound expands when heated at temperatures in excess of 300°C. By using the right amount of this material in the formulation, it may be possible to produce a matrix with controlled shrinkage or even expansion. Initial experiments with this material were inconclusive, though the metallographic section of one specimen showed a uniform, fine porosity. Although this method may not provide a quick solution to the problem, it has potential as a long-range solution and study in this area will be continued.

2. Pyrocarbon and Silicon Carbide Overcoats. It was felt that pyrolytic carbon or silicon carbide overcoats might be beneficial in two ways: first, they might provide a sacrificial overcoat that would fail leaving the particle coating intact and, second, they might provide a surface having less adhesion to the matrix. The overcoats were not successful in the first instance, but they were successful in the second. An overcoat of 3 μ of silicon carbide greatly reduced the adhesion of the particle surface to the matrix. The same effect was also produced to a somewhat less degree by a 3- μ overcoat of laminar pyrocarbon. A sacrificial overcoat of buffer-LTI was not successful. In most cases cracking either extended through the 2- to 10- μ overcoats and into the particle coating, or failed to prevent cracks that originated from the inside of particle coatings as a result of bearing stresses. However, a 2- to 4- μ overcoating of HTI over an LTI-coated particle resulted in noticeably less adhesion of the particle to the matrix, and thus appears to be the most practical at this time.

3. Surface Treatments and Resin Overcoats. Several different materials were tried as coated-particle surface treatments or overcoats to reduce the adhesion between particle and matrix. These materials were successful in varying degrees.

- a. KPR-2 Plastic. Treating the particles with KPR-2 plastic resulted in a tough plastic film that carbonized during the final heat-treat cycle. Very few cracked or broken particles (<0.33%) and a poor bond were noted. The rods had fairly good handling strength after carbonization and did not crumble or break, but problems were encountered in applying the plastic to the particles. The particles tended to stick together in groups after coating and were very difficult to separate.

- b. Teflon Mold Release. Teflon mold release showed little success in reducing particle-matrix adhesion, and significant numbers of broken coated particles were observed in the finished rods.
- c. Spraygraph - Graphite-Resin Overcoat. Treating the particles with Spraygraph produced an overcoat of resin-bonded graphite powder on each particle. Spraygraph, which is used as a film lubricant and mold release, consists of graphite powder suspended in a hydrocarbon solvent containing a small amount of resin binder. When particles coated with Spraygraph are incorporated in a fuel rod, carbonization leaves a film of graphite powder over the particle. This considerably reduced the adhesion to the matrix, and only a very few (<0.1%) broken particles were noted in a finished rod having a matrix containing 20% graphite powder and 10% charcoal powder as the filler material. Without the charcoal filler (30% graphite powder), approximately 2% of the particles had cracked coatings. No significant breakage was observed when a Spraygraph overcoat particle surface treatment was used on either HTI-coated particles or LTI-coated particles overcoated with HTI in a matrix containing 20% graphite powder and 10% charcoal powder filler (see Figs. 3.14 and 3.15). However, these treatment conditions did not appear to prevent cracking in particles used in the wet-molding process. Although the strength of the rod was also greatly reduced, there was enough particle-matrix bonding to give the rod sufficient handling strength. When applied to the particles, the Spraygraph dries readily, and the particles remain fairly free flowing. The few clumps were easily broken up.
- d. High-temperature Wax. With this treatment, a film of high-temperature wax was applied to the coated particles before they were incorporated in the fuel rod. In this process, 3 g of wax (Calwax 62A) was dissolved in a liter of trichloroethylene at 80°C, and the particles were then rinsed in the solution while it was still warm and allowed to dry. An injection-molded fuel rod fabricated with particles so treated showed a complete lack of adhesion between particles and matrix, and no broken particles were noted. The particles were completely loose and nested between loose chunks of matrix. The rod was so weak that particles crumbled off when it was touched. The treatment was repeated with less wax (1 g of wax per liter of trichloroethylene) on particles used in some wet-molded rods. This application was more successful: the rods showed no cracked particles and, though quite weak, were strong enough to be handled. Wet-molded rods were successfully fabricated with LTI-coated particles overcoated with 2 μ of HTI and with HTI-coated particles, both with the wax film overcoat.

Observations obtained from the metallographic examination of the various fuel rods fabricated with the different particle treatments and process parameters are summarized in Table 3.4.

Fig. 3.14--Rod 181: LTI-PyC-coated fissile and fertile particles overcoated with 2- μ HTI coating and Spraygraph mold release in injection-molded fuel rod with 70% phenolic resin binder - 20% graphite powder - 10% charcoal powder filler matrix (75X)

Fig. 3.15--Rod 182: HTI-PyC-coated fissile and fertile particles overcoated with Spraygraph mold release in injection-molded fuel rod with 70% phenolic resin binder - 20% graphite powder - 10% charcoal powder filler matrix (75X)

Table 3.4
RESULTS OF FUEL ROD EXAMINATION

Sample No.	Coated Particles	Overcoat Type	Process Type	Matrix Type	Diameter Change Through Carbonization (mils)	Strength of Fuel Rod	Broken Particles			Comments
							By Hydrolysis	By Met.		
								No.	Fraction	
126	LTI	None	IM	10% Char.	-0.6	Good	-5/in.	-	1/4-1/2	Many partially cracked coatings at bearing points; two particles with surface cracks where chunks of coating pulled out; good bond
102	LTI	None	IM	Standard	Not avail.	Good	>10/in.	-	1/3-2/3	Many badly crushed coatings through entire coating; good bond
3	HTI	None	IM	Standard	Not avail.	Good	>10/in.	-3	-1%	Two breaks at edge of bed; no increase in cracks after hydrolysis; good bond
143	LTI	KPR-2	IM	Standard	Not avail. ^a	OK for handling	-5/in.	1	-1/3%	Break occurred at edge of bed; nonuniform matrix; very poor bond
146	LTI	Laminar	IM	10% Char.	Not avail. ^a	OK for handling	-5/in.	0	0	Poor bond; uniform matrix
144	LTI	SiC	IM	Standard	Not avail. ^a	Crumbled easily in handling	-5/in.	2	-2/3%	Poor bond; uniform matrix
FOD-TEF	LTI	Teflon	WM	FOD standard	-3	OK for handling	5/6 (in 11-in.)	-	1/4-1/2	Most cracks only part way through coating; good bond; uniform matrix; some compound cracks
FOD-Std	HTI	None	WM	FOD standard	-4	Good	0 (in 11-in.)	-	1/3-2/3	Many catastrophic crushing failures, some with matrix in crack; good bond, porous matrix
147	LTI	Spraygraph	IM	10% Char.	0.9	OK for handling	0/in.	-	---	Crushed during hot-press mounting
157	LTI	Spraygraph	IM	Standard	0.7	OK for handling	0/in.	-	---	Crushed during hot-press mounting
151	LTI	Spraygraph	IM	10% Char.	0.8	OK for handling	0/in.	2	-2/3%	Bond good only occasionally; relatively low porosity matrix
155	LTI	Spraygraph	IM	Standard	1.2	OK for handling	0/in.	6	-2%	Poor bond but somewhat better than in Sample 151; bearing cracks usually occur where bond is retained; second cut showed three broken coatings

Table 3.4 (continued)

Sample No.	Coated Particles	Overcoat Type	Process Type	Matrix Type	Diameter Change Through Carbonization (mils)	Strength of Fuel Rod	Broken Particles			Comments
							By Hydrolysis	By Met.		
								No.	Fraction	
162	LTI	30- μ Buffer 2- μ LTI	IM	Standard	-6.5	Good	2-5/in.	-	1/4-1/2	Response to polishing indicated sample was very hard; overcoat pulled away only occasionally; not much indication of buffer crushing at bearing points; two or three structural coatings had chunks pulled out; good bond
166	LTI	Buffer-LTI (15-20 μ total)	IM	Standard	-1.9	Good	2-5/in.	-	1/4-1/2	Overcoat nonuniform from particle to particle; again only a few buffers pulled away; occasional crushing or buffer at bearing points; good bond and uniform matrix
168	LTI	Buffer-LTI (~5 μ total)	IM	Standard	-1.8	Good	2-5/in.	-	1/4-1/2	Same as Nos. 162 and 166 except that matrix very porous and cracked; most cracks only part way through coating; traces of nonuniformities in matrix
171	LTI	HTI-Spraygraph	IM	Standard	1.2	OK for handling	0/in.	(2)	(~1/4%)	Two breaks appeared to be due to grinding; several coatings subsequently broke due to hydrolysis; uniform matrix; fairly porous
172	HTI	Spraygraph	IM	Standard	1.9	OK for handling	0/in.	(0)	(0%)	No breaks observed until cores became obviously hydrolyzed; general appearance same as No. 171
181	LTI	HTI-Spraygraph	IM	10% Char.	1.5	OK for handling	0/in.	(1)	(~1/8%)	One break appeared to be due to grinding; again more breaks occurred after hydrolysis; uniform but porous matrix; intermittent bond
182	HTI	Spraygraph	IM	10% Char.	1.0	OK for handling	0/in.	(0)	(0%)	No breaks observed until cores became obviously hydrolyzed; general appearance same as No. 181
426	HTI	Spraygraph	WM	FOD standard	-3	Good	0 (in 11-in.)	(0)	(0%)	Some breaks observed which appeared to be due to polishing and subsequent hydrolysis; several large pores in matrix; somewhat better bond than for Nos. 171, 172, 181, and 182

Table 3.4 (continued)

Sample No.	Coated Particles	Overcoat Type	Process Type	Matrix Type	Diameter Change Through Carbonization (mils)	Strength of Fuel Rod	Broken Particles			Comments
							By Hydrolysis	By Met.		
								No.	Fraction	
402	LTI	HTI-Spraygraph	WM	FOD standard	-1	OK for handling	0 (in 11-in.)	(0)	(0%)	Same as No. 426
423	LTI	HTI	WM	FOD standard	-2	Good	0 (in 11-in.)	2	~1/4%	Some additional breaks due to polishing; fair bond; less porous than No. 426
430	LTI	Spraygraph	WM	FOD standard	-3	OK for handling	0 (in 11-in.)	-	10-30%	Some additional breaks due to polishing; apparently higher fuel loadings than No. 423 or 402; matrix does not appear to pull away from coating

^a Samples 137 through 146 jammed in bake holder and removed by sawing holder apart; final dimensions therefore lost. Low mechanical integrity and exfoliation contributed to inability to take dimensions.

A basic incompatibility arises when the necessity for low particle breakage is balanced against the need for handling strength. In order to reduce or eliminate breakage of the weaker particles, there must be little or no particle-matrix bonding, but when the particle-matrix bonding is reduced, the strength of the finished rod is also reduced. To solve this problem either a matrix must be found that has little or no shrinkage on carbonization (possibly by using expanding matrix, a long-range solution), or the particles must be overcoated with a material that will give carefully controlled particle-matrix adhesion (a short-range solution).

Thermophysical Properties. Previous data on the thermal conductivity of loose and bonded beds of coated fuel particles was summarized in GAID-8324 (Ref. 2), which is being prepared for publication.

Fuel Uniformity Statistics. Studies of the $\pm 1\%$ tolerance on Th and U in refueling regions were concluded with the preparation of a summary report, GA-8115 (Ref. 3), which is being processed for publication.

A statistical evaluation of available fuel uniformity data was conducted for the purpose of predicting long-term defect rates in PSC fuel rods. (A defect is a 1-in. segment of fuel rod deviating in fissile loading by more than $\pm 10\%$ from average fissile loading. It is assumed that parameters such as loading bias and packing fraction can be controlled.)

Loading data for both simultaneous metering (used in the injection-molding process) and wet mixing were evaluated and compared. In both cases a 1:1 weight ratio of fissile and fertile particles was desired. The data are summarized in Table 3.5.

Table 3.5
SUMMARY OF FUEL UNIFORMITY DATA

Loading Process	No. of 1-in. Segments Analyzed	Max. Observed Dev. from Avg. Fissile Weight (%)		Unbiased Std Dev. of Samples (%)
		Pos.	Neg.	
Simultaneous metering	90	2.1	1.8	0.863
Wet mix	19	9.5	6.0	4.67

The sample standard deviation and the chi-squared distribution functions were used to predict lower limits of the actual standard deviation associated with the loading process at various confidence levels. This means that since the sample standard deviation is only an estimate of the true standard deviation, the maximum available information about the true standard deviation is derived by using the chi-squared distribution to establish the confidence (or probability) that the true standard deviation lies above a given value, or

below a given value, or within a given range. This given value was used along with the normal distribution function to establish the probability of a given 1-in. segment being a defect (i.e., being out of tolerance). This probability is the long-term defect rate. In this way, a confidence that the true defect rate lies above a given value was established for several defect rate values.

The results of the evaluation are given in Fig. 3.16 for the wet-mix process. Figure 3.16 shows, for example, a high confidence level (90%) that the defect rate exceeds 1%. This defect rate is relatively high, and 100% inspection would probably be required even if a defect rate as low as 1% could be achieved. Taking the complement of the confidence level, it is further noted that one cannot even be 75% confident that the defect rate is less than 6%. If additional test data indicated that 4.67% was indeed a good estimate of the true standard deviation, one would anticipate a defect rate of about 3.5%.

A similar analysis of the simultaneous metering data revealed a high confidence level that the true standard deviation lies within a rather narrow range about the sample standard deviation as shown in Table 3.6. The $\pm 10\%$ tolerance is expected to be a $\pm 11\sigma$ or $\pm 12\sigma$ limit. The defect rate for such a tolerance is therefore essentially infinitesimal (about 10^{-23}), and the defect rate is expected (on a statistical basis) to be some 10^{21} times smaller than for the wet-mix process. It is therefore apparent that the simultaneous metering loading process results in a high confidence level with a sampling inspection to account for nonstatistical events (e.g., human error).

Table 3.6
RESULTS OF EVALUATION OF UNIFORMITY DATA ON
SIMULTANEOUS METERING PROCESS

Confidence Level ^a (%)	σ_c (%)	$10\sigma/\sigma_c$	D_c
99.5	1.064	9.40	5.46×10^{-21}
99.0	1.041	9.60	7.99×10^{-22}
97.5	1.010	9.90	4.16×10^{-23}
95.0	0.984	10.16	---
90.0	0.956	10.46	---
75.0	0.911	10.97	---
50.0	0.866	11.55	---
25.0	0.824	12.14	---
10.0	0.789	12.68	---
5.0	0.769	13.00	---
2.5	0.753	13.28	---
1.0	0.735	13.62	---
0.5	0.722	13.84	---
0.1	0.699	14.31	---

^aConfidence (c) that true standard deviation is below σ_c and that true defect rate is below D_c .

Fig. 3.16--Confidence level for various defect rates using the wet mix process

GRAPHITE DEVELOPMENT

Three full-size (18-in.-diam) logs of representative H-327 graphite have been received, and two of these are being sampled for thermal and mechanical properties. Three full-size logs of 9567 (Nuclear 2 purity) graphite have been ordered for a similar type of evaluation.

Work has begun on establishing a data-storage and selective-retrieval system for all data on PSC candidate graphites.

Control Material

Control Rod Absorber Material. During evaluation and development of absorber materials, it has often been necessary to determine the boron and carbon content of the materials. Past experience has shown that analysis of boron, boronated graphite, and B_4C to obtain accurate B and C content by volumetric and gravimetric techniques is difficult and relatively costly. Oxidation of B_4C to form a protective film of B_2O_3 impedes attempts to thoroughly oxidize specimens. Inhomogeneity of samples is another factor that makes conventional analysis difficult.

As part of this task an effort has begun to develop nondestructive analytical techniques. Investigations have been concentrated on the utility of neutron-activation analysis according to the following reaction:

One can expect approximately 1.3×10^6 counts Li^8 /sec-g boron when irradiated for 12 sec in the rabbit terminus in the TRIGA reactor. The high-energy beta emissions of the Li^8 are counted by means of a Cerenkov counter using the multichannel scaling mode of a TMC 100-channel analyzer.

Using the rabbit facility at TRIGA, a sample can be irradiated for several seconds and then delivered directly to the counting assembly in about 2 sec. Several samples have been analyzed for boron by activation analysis and the results compared with the chemical titration technique. This comparison is shown in Table 3.7.

Table 3.7
BORON ANALYSIS BY TWO METHODS

Sample No.	Boron (%)	
	Activation	Titration
1	26.6	21.9
2	21.1	22.3
3	32.8	32.7
4	31.3	30.8

The activation method offers a distinct advantage over the chemical technique in that it is nondestructive and quite rapid. Once reduced to a routine operation, the analysis per sample can be expressed in a matter of minutes. The precision and accuracy of this technique appear to be capable of achieving $\pm 1\%$. At present some difficulty is still being encountered in obtaining consistent performance from the system. These problems seem to have been related to the vial sample holders, but a newer vial design appears to have overcome the difficulty. Further testing is necessary before final assessment of the method can be made.

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. The formation of boron-laden dust during the shutdown might present a problem for later operation of the reactor.

Coating the B_4C granules with pyrolytic carbon might minimize or eliminate the dusting problem. The PyC coatings could also reduce any tendency of the B_4C granules to clog the hoppers, should this prove to be a problem. Plans are being made to investigate the feasibility of the PyC coating process with B_4C granules of the reference size.

Irradiation Testing

A series of irradiation experiments is being conducted to test reference 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. Proof tests are currently in progress to demonstrate the irradiation stability of reference BISO-coated particles under PSC reactor conditions. These experiments are being conducted at the PSC maximum fuel temperature to full design fuel burnup and to a significant fraction of the integrated fast-neutron dose.

Two fuel rod irradiation experiments in the ETR are being planned to evaluate the effects of the fuel rod matrix on coated particle stability at high-fast-flux exposures. 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 produce additional stresses in the coatings as 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.

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 PSC BISO-coated particles. The coated-particle samples were designed in accordance with current models of coated particle and pyrolytic carbon irradiation behavior to give a range of merit factor (Ref. 4) and strain rate (Ref. 5) 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 probable ETR flux variations. All samples are being tested on fissile size cores. Detailed descriptions of the coated particle samples were given in an earlier 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 in the PSC core will increase from 1100° to 1300°C during the first year (to about 7% FIMA), drop abruptly to 1100°C, 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 about 13% FIMA, at 1100°C to 20% FIMA, and at 1000°C for those samples that exceed 20% FIMA. The capsule is now scheduled to be irradiated for five ETR cycles to accumulate a fast-neutron dose of approximately 4.5×10^{21} n/cm². This is slightly over half of the expected PSC maximum dose of 8.5×10^{21} n/cm². The fuel burnups will center around the full PSC design burnup of 20% FIMA.

The P17 capsule is being irradiated in the 113NE core position of the ETR and is currently in its third cycle of irradiation. The capsule is operating very satisfactorily at near-design fuel temperatures and with most of the thermocouples still operative. Temperatures in two of the cells were lowered to 1100°C during the third cycle, and temperatures in the other cells will be lowered as fuel burnups reach an equivalent level.

The fuel temperatures are now 1050° to 1100°C in the 1100°C cells and 1250° to 1325°C in the 1300°C cells. The fuel has reached estimated burnups of 16%, 12%, and 10% FIMA (for the three Th:U ratios), with an estimated fast-neutron dose of 2.4×10^{21} n/cm² ($E > 0.18$ Mev). The capsule is scheduled to complete its five-cycle irradiation in February 1968.

Capsule P18. Capsule P18 is the second proof test of PSC BISO-coated particles. It is a longer-term test (about eight ETR cycles), concurrent with capsule P17, of similar samples designed to achieve a higher fast-neutron dose. The dose will be 6 to 7×10^{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 capsule P17) to simulate the temperature history of a typical PSC maximum temperature core region. All cells are now operating at a nominal 1300°C (PSC maximum temperature); however, some cells will be lowered to 1100°C at the start of the next cycle when the fuel burnups reach an appropriate level. A detailed description of the coated particle samples in capsule P18 was given in a previous quarterly report (GA-8038).

Capsule P18 is being irradiated in the ETR in the 113SW core position, and is in its third cycle. It is continuing to operate successfully at near-design temperatures of 1215° to 1325°C and with 95% of the thermocouples

still operative. The fuel burnups to date are estimated as 12%, 10%, and 9% FIMA for the three Th:U ratios. The estimated fast-neutron dose is 2.4×10^{21} n/cm² ($E > 0.18$ Mev). The capsule is scheduled to complete its eight-cycle irradiation in July 1968.

Capsule P19. Capsule P19 will be the third proof test of PSC BISO-coated particles. The objective of this capsule experiment will be to test BISO-coated particles prepared in large-scale (production) equipment. Both BISO-HTI and BISO-LTI coatings with several different density - anisotropy-factor combinations will be tested on fissile particles. Capsule P19, which will receive a six- to seven-cycle irradiation in the ETR, is designed to achieve full PSC fuel burnup of 20% FIMA for fissile particles with a fast-neutron dose of about 6×10^{21} n/cm² ($E > 0.18$ Mev). The design fuel temperatures are 1300° and 900°C, and they will again be programmed (as with capsules P17 and P18) to simulate typical PSC maximum and average temperature core regions.

Fabrication of the capsule components is continuing. A description of the capsule design is given in Ref. 6. Selection of the coated particle samples is in progress.

Capsules FR-1 and FR-2. The objectives of the two fuel rod capsule irradiation experiments are to 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 alongside each fuel rod for a direct comparison of coated particle performance loose and in the rods.

Preliminary capsule design studies have shown that the present P-capsule design can be modified to accommodate five fuel rods. One fuel rod would be contained in each cell of the capsule, and the sample of loose coated particles will be in a smaller hole in each cell. By using fertile particles of all ThC₂ and fissile particles of the proper thorium-uranium ratio to yield 20% FIMA burnup, a proper ratio of fissile and fertile particles to simulate PSC design conditions would be obtained while still achieving the correct uranium loadings for capsule operation.

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. Adequate thermocouples will be included in the graphite around the fuel rod to give a good temperature history of each sample.

A number of fuel rod types are candidates for these experiments, and improved materials are expected to come from the development program during the next few months. Current candidates include fuel rods prepared by the present injection molding process with both the standard matrix and the 10% charcoal matrix (the types of fuel rods being prepared for the Fort St. Vrain proof test elements). Consideration will also be given to rods made with expanding fillers, some made by the wet-molding process, and possibly ones made with natural flake graphite fillers whose long-term irradiation behavior might be superior.

Postirradiation examination of the fuel rods is expected to center on dimensional stability, coated particle integrity, interaction of the fuel rods with the graphite containers, and fission-product release tests.

The first experiment, FR-1, is scheduled to begin irradiation in the ETR in May 1968, and it is planned for a fast-neutron dose of 4 to 6×10^{21} n/cm² ($E > 0.18$ Mev). The second experiment, FR-2, is expected to begin irradiation one cycle later (June-July 1968), and it will be irradiated to a higher fast dose (7 to 8×10^{21} n/cm²). Detailed design work is in progress on both capsules.

Capsule G12. Capsule G12 was designed to irradiate specimens from large-diameter logs of PSC candidate graphites. Needle-coke graphite grades H-327, 9567 (Nuclear 3 purity), and TS-688 were tested. A description of the materials has been given in an earlier quarterly report (GA-7634).

The capsule has completed seven cycles in the ETR, with the sample receiving estimated exposures of 3 to 6×10^{21} n/cm² ($E > 0.18$ Mev) at 600° to 1000°C . Postirradiation examination is now in progress.

Capsule G13. Capsule G13, a follow-on to G12, contains needle-coke graphite grades H-327, 9567 (Nuclear 2 purity), and TS-688, as well as nominally isotropic grade RC4. The capsule 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 5×10^{21} n/cm² ($E > 0.18$ Mev). Capsule cell No. 2 has been running at about half of the design temperature of 600°C , and during the first ETR cycle one of the internal thermocouples in this cell became erratic and inoperative. The remaining three test cells have been functioning well; temperatures are near the design values of 600° and 1000°C .

Dimensional Changes in Irradiated Graphites. A survey of the literature was made to evaluate the possibility that irradiation-induced dimensional changes may in some manner depend on the size of the test sample. Although there are some published data that have been attributed to such a size effect, the evidence is not at all conclusive because of the many variables that affect the irradiation behavior of graphite. Graphite varies significantly from log to log and somewhat from position to position within a single log as a result of differences in degree of preferred orientation, density, etc. Also, irradiation conditions vary considerably from one experiment to the next, so that it is frequently difficult to compare results from different experiments. Accordingly, it would be inappropriate to make allowances for a size effect in fuel element design based upon present data unless such an effect is verified by more definitive data and its magnitude is established.

Another survey was made of the turnaround in the dimensional changes of irradiated graphites in the parallel direction. Results show that the irradiation-induced contraction in the parallel direction may saturate and then reverse or turn around. However, this turnaround does not occur within the range of temperatures and exposures of interest to the PSC plant.

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.
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

Determination of Fission Cross Section for Thorium. A determination of the fission cross section for thorium in the TRIGA Mark I reactor has been performed. This determination is required for the evaluation of pyrolytic carbon-coated thorium-carbide (fertile) particles in a manner similar to that used for the evaluation of fissile particles. The steady-state release of fission gases from these particles will be measured to determine the quality of the coatings with regard to permeability of the gases and thorium contamination levels.

Calibration runs were performed with the TRIGA King furnace in the Mark I TRIGA reactor. Samples of thorium stearate were fissioned at room temperature, and data were obtained for each fission gas of interest ($\text{Kr}^{85\text{m}}$, Kr^{87} , Kr^{88} , Kr^{89} , Xe^{138} , and Xe^{139}). Yields were obtained in terms of counts per minute per fission. The calibration is based on the assumption that the fatty-acid compounds, in the form of noncrystalline powders, exhibit 100% release of the fission gases with half-lives that exceed one second.

Results show that the fission cross-section ratio of normal uranium to thorium in the F-ring of the TRIGA Mark I reactor is 300:1. Assuming that Th^{232} is the fissioning species, and that 3.97 barns is the fission cross section of normal uranium with thermal neutrons, the thermal neutron fission cross section of Th^{232} is 0.013 barns. This confirms a value of 0.011 barns

obtained several years ago when a ceramic specimen of high-density ThO_2 , containing a trace of depleted uranium, was studied in the TRIGA Mark I reactor. These values may be compared with the reported value of less than 10^{-4} barns for the Th^{232} fission cross section (Ref. 7).

Postirradiation Annealing Experiments. The release of iodine and other fission products from both intact and cracked irradiated particles is being studied to provide information for the safety analysis of the Fort St. Vrain reactor. An objective of the study is to measure the iodine release from particles as a function of burnup and to account for the iodine inventory by also measuring the retention.

Iodine release measurements on both intact and cracked pyrolytic carbon-coated fuel particles have been performed using a TRIGA King furnace inserted in the F-ring of the TRIGA Mark I reactor. In each experiment approximately 20 fuel particles were placed in the furnace and irradiated to 500 kw-hr at room temperature to give measurable activities of 6.7-hr I^{135} . Immediately after the irradiation, the particles were annealed for 6 hr at 1000°C (or other desired temperatures) and the system was purged with helium during the anneal at 2 l/min. The furnace was then turned off, the cold trap was changed, and the helium purge was continued. The helium purge gas flowed out to a liquid-nitrogen-cooled charcoal trap. After 8 hr of purging, the effluent trap was measured for 9.2-hr Xe^{135} (I^{135} daughter) activity to determine the fractional release of iodine during the anneal.

To obtain a material balance for the iodine, the particles were burned at 1000°C in a 10% oxygen/90% argon atmosphere after the completion of the annealing experiments. Gases released in the burning operation were collected in a liquid-nitrogen-cooled trap.

The results of measurements of the release and retention of iodine during annealing of intact PyC-coated particles (irradiated to 24% FIMA burnup) were reported in the previous quarterly report. These data showed that (1) the release of iodine from intact, irradiated particles was less than 10^{-3} in 6 hr at 1340°C and very probably significantly less, (2) the particles retained the total iodine inventory within the precision of the measurements, and (3) the inventory of the xenon daughter of the iodine could also be fully accounted for.

The iodine release studies have been extended to include measurements on cracked irradiated particles as a function of burnup.

The process of producing fine cracks in individual fuel particles has, in the past, been a difficult, time-consuming, and expensive operation. Also, the spread of contamination has been a problem. To overcome these problems, a fuel particle cracking apparatus with wide versatility has been designed, fabricated, and put into use.* The apparatus is shown in Fig. 3.17. The particle to be cracked is enclosed in the fold of a sheet of aluminum foil

*This apparatus can also be used to "shell" the particles, that is, to separate the coating from the kernel.

Fig. 3.17--Fuel particle cracking apparatus

Fig. 3.18--Sample of cracked fuel particles

and placed on the lower anvil. The upper micrometer head forces the upper anvil down onto the particle against the upward thrust of two heavy springs that support the dial-gage platform. Contact between the upper anvil and the particle is indicated by movement of the dial gage, which has a maximum travel of about 62 μ . Micrometer motion beyond this limit puts increasing stress on the particle until the coating fractures. Fracture of the coating is detected audibly by a small microphone attached to the dial gage. Using this instrument, fine hairline cracks can be induced in the coatings rapidly, with minimum personnel exposure, and with no spread of contamination to either the other particles or the apparatus. Several cracked particles are shown in Fig. 3.18. The improved capability has led to more stringent criteria for acceptability. During the quarter about 120 particles have been cracked with a yield of particles of the quality shown in Fig. 3.18 of about 90%.

To date, iodine release experiments have been conducted on seven samples of cracked, irradiated particles with burnups ranging from 4% to 24% FIMA. Also, iodine release from uncoated kernels was measured. The release data and a description of the samples are given in Table 3.8. The fractional release of iodine does not appear to be dependent on burnup in the range 0% to 24% FIMA. Inventory analyses are being performed.

Sorption and Diffusion of Fission Products

Strontium-Graphite Dust Sorption Experiments. A long-term strontium sorption experiment using the "Sr Dust B" method described in previous quarterly reports 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 tubes were coated with Sr^{85} -tagged graphite dust. The tubes are being maintained in a thermal gradient ranging

Table 3.8

IODINE RELEASE FROM UNCOATED AND CRACKED PARTICLES

Sample No.	Kernel	Coating	Burnup (% FIMA)	Annealing ^a Temp (°C)	Fractional Release of Iodine
P1701ESR-1	150- to 250- μ (Th,U)C ₂ Th:U = 1.6:1	Uncoated kernel	0	1000	0.008
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
3023-77E	177- to 250- μ (Th,U)C ₂ Th:U = 4.0:1	29- μ buffer, 68- μ isotropic	7.9	1000	0.02
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
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
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

^aTime at temperature was 6 hr.

Task IV

FUEL ELEMENT PROOF TESTING

The objective of this subtask is to design, fabricate, irradiate and subsequently examine a proof test fuel element for the Fort St. Vrain reactor. The element will be tested at expected plant operating conditions including expected fuel rod temperatures and greater than expected fuel rod temperature gradients. The fuel element contains graphite and fuel particles of the same type that will be used in the PSC core and will be irradiated to one-third the expected PSC core burnup.

The previous quarterly report (GA-8270) contains a detailed description of the design and expected performance characteristics of the proof test element.

During this reporting period, the fabrication and assembly of the test element was completed, and the element was shipped to the Peach Bottom site for irradiation. The reactor is presently scheduled to be shut down during January 1968, at which time the test element will be loaded into the core.

Prior to the shipment, a complete checkout of the shipping container and packaging method was completed. A dummy element was loaded into the shipping cask and subjected to handling and shock loads similar to those that could be expected during shipment of the element. The element was then removed from the cask, inspected, and found to be in excellent condition.

During the period a demonstration model proof test element handling tool and a parking hole canister were designed and constructed for use at Peach Bottom. These items, along with a 5-ft dummy proof test element and 22 shortened dummy Peach Bottom elements were used for a fuel handling demonstration and for the purpose of developing detailed procedures for loading and unloading the proof test element with the fuel transfer machine.

After the demonstration test the design was finalized for the proof test fuel element handling tool and the tool latch block. An insert was also designed to fit into a standard Peach Bottom parking hole canister. This insert will position the handling tool within the center of the canister for ease of fuel transfer machine operation and so that both the canister and tool can be taken into or out of the reactor together. In addition, a fuel handling tool storage container was designed and constructed for use at Peach Bottom to enable the proof test element fuel handling tool to be protected while in the canister storage area between tool use.

FUEL RODS

The fuel rod fabrication processes were modified to prevent the cracking of particle coatings discussed in Task III of this report. The particles had HTI coatings in some rods, and LTI coating or SiC coating overcoated with HTI in others. All types of particles were overcoated with Spraygraph coloidal graphite mold release. The particles in wet mix rods had a final wax film overcoat from a 1-g/l wax - trichloroethylene solution. The majority of the wet-molded rods had the 20% graphite powder filler matrix.

Using these processes, a total of 209 injection-molded rods and 12 wet-molded rods were fabricated for the proof test element. This represents a total of 1298 in. of fuel rod, (994 in. being required for the complete element). Four types of coated particles, HTI, LTI, and two SiC coatings were used along with two types of matrix filler (30% graphite powder and 20% graphite powder - 10% charcoal powder) for each of the two processes.

PIGGYBACK SAMPLES

Piggyback samples of various fuel and graphite materials were prepared for irradiation in the proof test element. These samples will allow comparison of the behavior of various component materials to that of the composite fuel rods and graphite elements. They also allow performance of these materials to be determined in an HTGR flux spectrum rather than in a water test reactor spectrum.

The samples being tested include:

1. Specimens of coated particles of each of the nine types being used in the fuel rods (LTI, HTI, and two SiC types on fissile and fertile particles).
2. Matrix material of each of the four types being used in the fuel rods (injection-molded with phenolic resin binder and both graphite and graphite - 10% charcoal fillers).
3. HTI PyC, LTI PyC, and SiC coatings, both restrained and unrestrained (including both samples of coatings used on particles in fuel rods and other samples of interest for comparison with P-capsule results).
4. Graphite samples of current interest for PSC (including 9567, H327B, TS-688, and RC4).

The samples are in two locations in the element: fuel zone 3 and fuel zone 2 (see Fig. 4.2 of the previous quarterly report). The samples in zone 3 are located in the center of annular fuel rods. The samples are 1/8 in. in diameter and occupy a total length of 17 in. They will operate at a temperature of 1150° to 1200°C, which will be monitored by a nearby thermocouple, and they will receive a fast-neutron dose of about 1.3×10^{21} n/cm² ($E > 0.18$ Mev). This zone contains all four types of samples described above. The samples in zone 2 are disks that go between the fuel rods. These disks are made of the various graphites described in 4 above, and each is nominally 1/2 in. in diameter and 1/8 in. thick. They will operate at a center

temperature of 1000° to 1150°C with a radial gradient decreasing to 600° to 700°C at the edge. They will receive approximately the same fast dose as the samples in zone 3.

All samples were completed and loaded into the element.

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 this period concentrated effort has been applied on the task of preparing the detail drawings of the fuel transfer machine for release for manufacture. At this time all drawings for the grapple head have been released for fabrication. Material procurement and fabrication are well under way.

A preliminary design layout of the fuel handling machine control room has been completed. This drawing gives a proposed dimensional layout of the operating console, the digital computer, and the electric cable trench in the floor.

The design of the analog servo system is near completion, and specifications have been prepared for the procurement of the servo packages.

From thermal analyses of the machine internals that operate in the reactor core region, it has been determined that all the selected electrical and electronic components, so mounted, will meet the temperature and radiation tolerance requirements. This conclusion was based on a total gamma dose of 1.4×10^8 rads and a 350°F environment.

The design of the fuel handling machine control system is in progress. The PDP-8/S computer manufactured by Digital Equipment Corporation has been received, and a preliminary programming is in progress including an evaluation of program control and methods of data transfer and storage. Computer interface and external digital logic design is continuing and breadboard control circuits are being constructed.

An electrical diagram of the internal wiring of the fuel handling machine has been updated to include terminals to accommodate the television cable near the dome (interior) section and the television camera lighting change from four lamps to five. The lamp change was to provide a greater intensity during normal viewing. The lighting at this time provides three 500-w quartz-iodine lamps for normal viewing and two 500-w quartz-iodine lamps for vertical viewing. All lamps will have independent brightness control.

For the planned grapple head test program, a test rig and autoclave have been designed, and manufacturing drawings are being prepared. The autoclave is designed to simulate typical temperatures in helium that are expected to

be encountered in the actual refueling operation. The test rig mechanism is designed to provide vertical motion that will allow the grapple head to lift and lower the dummy fuel element and laterally shift the element to check the built-in capability of the grapple head to locate and grasp under an offset core condition. Since the actual fuel elements will be hotter than the surrounding gas during refueling, the test fuel block is provided with internal heaters to simulate heat conduction into the contacting parts of the grapple head.

Manufacturing drawings for the remaining portion of the fuel transfer machine are in the process of being checked and reviewed prior to release for fabrication. Potential suppliers for items to be procured from outside vendors are being established from inquiries based on preliminary drawing information.

Activity is well under way for the planned over-all fuel handling test program. Architect/engineer drawings for the structural modifications to the test tower are in process at this time. A purchase order for the 15-ton capacity bridge crane has been released. The quoted delivery date for the crane means that it will have to be installed after the test tower structural modifications. These modifications consist essentially of raising the tower height by 25 ft, providing the steel structure for supporting the fuel transfer machine, and relocation of certain access landings.

Activity has started in the design of the simulated core internals for the test autoclave. The plan is to build up a core section that will represent a normal fuel region of seven fuel columns, a small border region of five fuel columns and four reflector columns, and a large border region of seven fuel columns and three reflector columns. Consideration is being given to building the core section only half of the normal height, since the fuel transfer cask can only store one half of a normal region. The dummy core in this case would then be provided with means to allow it to be raised and lowered, so that fuel handling at all core elevations can be tested.

Design activity is also under way on the mounting ring structure for supporting the fuel transfer machine during test. The mounting ring is interposed between the fuel transfer cask bottom flange and the test rig support structure. The mounting ring supports a nozzle extension containing an access door, which will permit inspection of the fuel transfer machine grapple head, the arm index mechanism, and the television camera. Without the nozzle port, it would be necessary to cool down the autoclave and enter that region for access to these components. An isolation valve is therefore located inside the autoclave that seals against the lower end of the refueling sleeve. The valve is a simple flapper type, similar to that successfully used on the test of the Peach Bottom fuel transfer machine.

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

Part of the rotor inertia simulator test program included tests with various amounts of unbalance. The purpose of the tests was to determine the rotor amplitude response within the operating speed range. Tests were conducted with 0.20 in.-oz unbalance (which is the standard maximum limit for the production rotor) and with 1, 3, and 6 in.-oz unbalances placed in the plane of the compressor wheel.

The rotor vibration with standard unbalance (0.20 in.-oz) throughout the entire speed range (0 to 10,000 rpm) amounted to less than 1×10^{-4} in. radial displacement, which is low relative to the radial clearances of the upper and lower journal bearings (these are 3.4×10^{-3} in. and 2.35×10^{-3} in., respectively). With 3 and 6 in.-oz unbalance, the level of amplitudes increased. The maximum rotor amplitude was noted to occur at approximately 7000 rpm (i.e., within the operating range) as shown in Fig. II.1. The measurement of the relative phase angle between the maximum amplitudes at two bearings and the phase angle of the unbalance load made it possible to reconstruct within the accuracy of the instrumentation the exact rotor mode of motion and its phase relation to the unbalance force at every speed. Fig. II.2 shows this relationship plotted versus rotor speed. Since the rotor bending was found to be negligible throughout the entire speed range, the rotor was treated as a rigid body on flexible supports.

From the summation of all the inertia forces and moments acting on the rotor at speed increments of 1000 rpm, it was possible to calculate the resultant bearing reactions, and from these the bearing spring rates and damping coefficients that are related to the forces due to displacement and to the forces due to velocity. The spring rate of the lower journal bearing based on this analysis is shown in Fig. II.3 as a function of speed.

From the spring rates and damping coefficients obtained from the test data, it was found that at least 12 in.-oz unbalance could be taken at the compressor wheel plane without exceeding the eccentricity ratios of 0.85 at the turbine bearing and 0.40 at the compressor bearing. (The eccentricity

Fig. II.1--Unbalance test runs on the rotor inertia test rig

Fig. II.2--PSC rotor 3 in.-oz unbalance

Fig. II.3--Turbine bearing; spring rate versus speed

ratio is defined as the ratio of amplitude to clearance.) If the non-linearity of spring and damping coefficients at high eccentricity ratios is allowed, it is estimated that that amount of unbalance could be as high as 20 in.-oz with the present bearing configuration before rubbing would occur. However, it was concluded that it would be worthwhile investigating the possibility of increasing the stiffness of the journal bearings, especially of the turbine-side bearing, in order to shift the maximum amplitude response completely out of the circulator operating range and to increase the unbalance capacity.

Further work on increasing the stiffness without sacrificing the damping properties of the lower (turbine-side) journal bearing is now in progress. It is estimated that an increase in bearing stiffness by a factor of four will shift the maximum amplitude response from 7000 rpm to approximately 14,000 rpm. Analytical work based primarily on the available test data from the Valmont bearing test rig has shown that such an increase in bearing stiffness is feasible.

Two basic factors contributing to bearing stiffness were evaluated: (1) bearing pocket geometry and (2) individual pad orifice compensation. These two parameters were evaluated separately, and work is now in progress to optimize the combination of these two effects. There is strong evidence that an increase in stiffness by more than a factor of four will be possible.

In carrying out these studies of turbine bearing stiffness, comparisons were drawn among data obtained by three different methods. Figure II.3 shows a comparison of data obtained by the three methods.

1. Data predicted by the analytical computer program of Mechanical Technology Incorporated.
2. Spring rates based on displacement components of bearing reactions. The bearing reactions were calculated on the basis of rotor motion recorded for 3-in.-oz unbalance test runs.
3. Spring rates based on a correlation of the test data for the thrust bearing pad stiffness.

In addition to the generally higher spring rates predicted by the computer program, the difference in slope between predicted curves and curves calculated by methods 2 and 3 should be noted. The computer program predicted much higher bearing dynamic effects than those calculated on the basis of the test data (second and third methods).

Comparison of the predicted and actually measured pressure profiles on the upper journal bearing (see Fig. II.4) also show much higher dynamic effects for the predicted data. It appears that the small pocket depth is causing a large pressure drop between the supply channel and the pocket. This drop and the small pocket width seem to be the main reasons why the actual spring rates were lower than predicted. It was concluded from this comparison that the present analysis is inadequate, but there is no theory now available for reliable prediction of hybrid journal bearings with pockets

Fig. II.4--Upper journal bearing; tangential pressure distribution at $\epsilon = 0$

available for reliable prediction of hybrid journal bearings with pockets operating in a highly turbulent regime with high inertia effects. In order to optimize the pocket geometry, it was decided to rely on the third method and to use the available thrust bearing data.

The thrust bearing data basically consist of thrust measurements under given operating conditions (land clearance, speed, and supply pressure). Also, there are available plots of tangential and radial pressure distribution for each operating condition. Integration of pressures over the area gives the load at each of the conditions. Composite plots of some of these points are shown in Figs. II.5 through II.7.

It was possible to calculate the journal bearing spring rates with reasonable accuracy from the thrust bearing spring rates obtained from tests by assuming a journal-bearing slipper geometrically similar to the thrust-bearing slipper, matching the mean sliding velocities and the Reynolds numbers. The centrifugal pressure gradient of the thrust-bearing slipper was found to be negligible as compared to the tangential pressure gradient and was neglected in analysis. Figure II.5 shows a carpet plot of the thrust-bearing performance. Figures II.6 and II.7 show the rate of change of thrust load as a function of bearing displacement (clearance) for various rotational speeds and a given supply pressure. For pocket supply pressures of approximately 200 psi, these curves show that the range of maximum spring rates occur at the pocket-depth-to-land-clearance ratio (Δ/C) of approximately 4.5. Figure II.8 shows the thrust-bearing spring rate as a function of speed for Δ/C of 4.5 and 1.915 (the present turbine journal bearing has $\Delta/C = 1.915$). Figure II.9 shows the turbine journal spring rate versus speed for a constant pocket supply pressure of 200 psi and various Δ/C ratios. These curves were derived from curves (see Figs. II.6 and II.7) by assuming geometrically similar slippers, equivalent sliding velocities, and approximately equal Reynolds numbers.

As shown in Fig. II.9, an increase in pocket-depth-to-radial-clearance ratio (Δ/C) from 1.915 with present configuration to 4.5 will increase the stiffness by a factor of about three. A further increase in Δ/C parameter over 4.5 causes a drop in the spring rates as shown for $\Delta/C = 6.25$.

Evaluation of the orifice compensation effect on a strictly hydrostatic bearing of the same geometry as the present turbine journal (except for the large pocket depth) is shown on Fig. II.10. The contribution of this effect is on the order of 250,000 lb/in. increase in stiffness for low eccentricity ratios.

The two effects, pocket modification and orifice compensation, cannot be directly added because of the complex relationship between hydrostatic and hydrodynamic effects in a hybrid bearing. For example, the load capacity at zero speed usually is close to being proportional to the bearing supply pressure, but at an appreciable speed the effect of the supply pressure is less. Further work will be conducted on the combined effect using available test data.

Since the optimum Δ/C (or pocket depth for a given radial clearance) is not greatly affected by the pocket supply pressure (compare Figs. II.6

Fig. II.5--Main thrust bearing performance thrust versus mean Reynolds number

Fig. II.6--Main thrust bearing; thrust versus land clearance, $P_s = 200$ psi

Fig. II.7--Thrust versus land clearance for main thrust bearing, $P_s = 50$ psi

Fig. II.8--Main thrust bearing spring rate for constant supply pressures

Fig. II.9--Turbine journal bearing spring rate; Δ/C = pocket depth/radial clearance; ($C = 0.00235$ in.)

Fig. II.10--Effect of individual pad orifice compensation on load capacity of existing turbine bearing (large pocket depth)

and II.7), it can be concluded that the orifice compensation will not have an appreciable effect on pocket optimization. It is therefore planned to increase the width of the present lower journal bearing pockets to achieve geometric similarity with the thrust bearing pockets. The pocket depths will be increased, resulting in a Δ/C of about 4.5 for both bearings. It is also planned to leave the land width the same for the lower journal bearing in order to prevent a reduction of damping coefficient. This will increase the bearing width for approximately 1/2 in., which was found to be acceptable in view of the space available.

The compensation orifice area has yet to be optimized on the basis of the flow characteristic for the modified slipper, and the net effect on stiffness has yet to be evaluated.

Bearing designs in current use frequently involve steps in flow areas from pockets to lands and vice versa. Inertia effects are certainly important, and perhaps predominant, at such discontinuities. Since the local pressure changes determine the behavior of the fluid films on both sides of the step and the load carrying capacity of the bearing, they must be taken into account in rational analyses of bearing designs.

References 1 and 2 appear to provide the most up-to-date analysis and review of some of the problems discussed here, and in part may provide the methodology for their treatment. However, it is clear from even a cursory review of these papers that the extension and application of the theory to the problems of optimizing an actual bearing design within the constraints on that design imposed by broader considerations would require a significant applied research effort.

The data accumulated by Gulf General Atomic in this test program can serve as a starting point for a broader investigation, the results of which would be useful to other programs. Anything less than such an effort would require an extensive test program similar to that made with the rotor inertia simulator.

PROTOTYPE CIRCULATOR TESTS

The Phase II prototype circulator aerodynamic air tests were completed during the early part of the quarter. The data from these tests will permit performance evaluations of the compressor inlet, blading, and diffuser for the range of conditions from 25% to 144% of design aerodynamic speed at various flow rates. Special tests were conducted to determine the compressor shutoff head for equivalent aerodynamic speeds of 25%, 40%, 60%, 80%, and 100%. As previously reported, the compressor inlet had been calibrated and was used as a flow measuring device for these Phase II tests. The aerodynamic speed is defined as the ratio of blade rotational speed to the gas inlet sonic velocity. Testing at equivalent aerodynamic speeds compensates for the effect of Mach number on performance. The aerodynamic design speed in helium is 9550 rpm, but for air at 70°F and 12 psia, the mechanical speed needed to give the design aerodynamic speed is 2160 rpm.

A computer program has been written to assist with the data reduction of the Phase II aerodynamic air tests. There are approximately 370 input variables to be read into the program for each test condition including manometer tube levels, probe traverse plot data, barometric pressure, loop temperature, circulator speed, and Pelton turbine flow rate. The manometer levels are read in inches of water, with the manometer panel tilt angle and the reference levels as additional input. The probe plot data is given for 10 radial stations in millivolts of output.

The corrected manometer levels are determined and the measured pressures in pounds per square inch are calculated by the program. Then the compressor flow rate, pressure rise, diffuser efficiency, and other pertinent values are computed.

The probe data are reduced by using the program's library of calibration data to convert the millivolt readings to pounds per square inch and angular degrees. The pitch and yaw flow angles and the absolute and axial flow velocities are then computed for each radial station of each probe.

Figure II.11 is a photograph of a probe actuator mechanism during calibration. After calibration, the actuator is mounted to the side of the test vessel with the probe extending inward a distance of 5 to 29 in. depending on the annular flow path being monitored. Four or five such mechanisms were in use at various locations for each test condition. The probe can be positioned radially by a remote, automatic control. When a desired radial position is reached, the probe will react to the flow by sensing static pressure at two locations and balancing them through a feedback control system, thereby determining the yaw angle of the flow. Total pressure is also measured; therefore, the flow velocity can be calculated. Two other pressure taps on the probe provide readings from which the pitch angle of the flow can be determined. Figures II.12 and II.13 represent typical probe data taken during the Phase II aerodynamic air tests at test condition 39. The yaw and pitch angles versus radial position are plotted in Fig. II.12, and static and total pressures versus radial position are shown in Fig. II.13. These probe readings were taken approximately 3.5 in. upstream from the compressor rotor blade centerline.

Figures II.14a-d and II.15 a-d show noise spectrum data taken during test condition 39 of the Phase II aerodynamic air tests. The series of spectra shown in Fig. II.14 represent data from the lower microphone, which was mounted adjacent to the flow stream at the stator discharge (Z160 location). The spectra shown in Fig. II.15 show data from the upper microphone, which was installed at the diffuser discharge. The peak noise levels for this condition were 119 db, measured at 745 cps and 118 db at 1160 cps, both at the upper microphone. The frequency of the second/peak coincides with the number of stator blades times the rotational speed of the compressor ($33 \text{ blades} \times 36 \text{ rpm} = 1188 \text{ pulses/sec}$). Although the cause of the noise peak at 745 cps has not yet been determined, it appears to be associated with some part of the test apparatus. The frequency is constant in all the various runs at different speeds and air velocities. Furthermore, it was not present during the flow meter calibration tests, which suggests that it may be associated with the instrumentation rake at the diffuser discharge which was installed for these Phase II tests.

Fig. II.11--Probe actuator mechanism during calibration

Fig. II.12--Typical prototype circulator air test probe data. Yaw angle and pitch pressure as a function of radial position

Fig. II.13--Typical prototype circulator air test probe data. Total and static pressure as a function of radial position

(a)

(b)

Fig. II.14--Circulator air test noise spectrum data

(c)

(d)

Fig. II.14--Circulator air test noise spectrum data

(a)

(b)

Fig. II.15--Circulator air test noise spectrum data

(c)

(d)

Fig. II.15--Circulator air test noise spectrum data

Presently the raw data values for each of the 65 test conditions are being read prior to computer calculation of the results. Preliminary analysis of the data at 100% equivalent aerodynamic speed (2160 rpm in air) has produced the results illustrated in Fig. II.16, which is a plot of normalized compressor weight flow versus circulator static pressure rise ratio (from the circulator inlet entrance to the diffuser discharge). The test data agree with the predicted performance for this particular speed line, with the exception of the sharp decline in pressure rise rate below a flow of 12.8 lb/sec. Test data indicate that a flow separation along the inlet contour near the compressor rotor blade tip occurs as flow is reduced below 12.8 lb/sec. It is believed that this flow separation is induced by an adverse pressure gradient across the rotor blading, which is characteristic of most axial compressors when operated at similar conditions. This characteristic is usually referred to as compressor blade stall.

The indicated compressor blade stall point is sufficiently far removed from the typical compressor operating line to present no problems during normal operation. However, a desirable extension of the compressor operating range is possible if the wall stall could be eliminated or significantly reduced. Two fairly simple modifications are being considered for future developmental evaluation.

The first modification would be to install a splitter plate or suction ring along a streamline of the unseparated inlet flow near the compressor rotor shroud. This would be expected to reduce the extent of the separation by continuing to supply flow sufficiently close to the separated area so that this flow can then supply energy into the boundary layer, thereby postponing separation.

The second modification would be to install vortex generators on the shroud upstream from the present separation point. These vortex generators would be several times the boundary layer thickness and would be shaped so as to increase the mixing between the external and boundary layer flows. With augmented mixing, it is expected that separation would be delayed and the extent of separation, once it occurs, would be reduced.

Both modifications involve a small, probably negligible, reduction in compressor performance at design conditions. Investigators having previous experience with such devices consider them quite effective, but there are no data directly applicable to this particular configuration. To determine the effect of these devices on compressor performance, a separate test program would be necessary. It has also been concluded that revisions to the inlet within the geometric limitations of the prototype would probably prove ineffective and that revision of the compressor blading would neither be practical nor provide sufficient change in the off-design operating characteristics without seriously compromising compressor performance within the normal operating range.

PRIMARY COOLANT SHUTOFF VALVE

Two concepts for the primary coolant shutoff valve are being pursued. The first design employs two plates hinged from a common centerline (forming

Fig. II.16--Comparison of prototype circulator for data with predicted performance. Condition 39 and the equivalent design point are shown

a type of butterfly valve), linkage, pneumatic actuator, and associated instrumentation and controls. The two plates are of rugged construction that is compatible with the actuator loads and that would withstand the differential pressure across the plates when the valve is closed. This concept is presently considered the reference design.

The alternative design employs two plates similar to those of the reference design. No actuator is provided, and the plates are relatively light to reduce frictional resistance and improve response to aerodynamic loads, i.e., act as a check-valve. Radial support ribs support the plates when the valve is closed.

In order to support the development of both designs, a study of the reference design is being made by a vendor.

The alternative design originally employed heavier plates that were spring-counterbalanced. A valve of this design was manufactured and tested in air. Since the shape of the test valve plates is common to all designs, most of the aerodynamic test results are applicable to the other design concepts.

It was shown by this test that the friction in the pivots associated with the spring-counterbalance impairs valve performance. The friction in the hinges had a much smaller effect. As a result of these observations, the design of the alternative concept described above was initiated. The light-weight plates eliminate the need for a counterbalancing system and reduce the hinge frictional resistance. A test valve of this configuration is presently being manufactured.

VALMONT TEST FACILITY

Manufacture of the steam/water spool piece was completed and shipped to the Valmont test site during this reporting period.

The structural steel platform in the test tower at Valmont (elevation 125 ft-10 in.) was reworked to simplify the steam/water pipe installation.

The steam blowdown of the steam lines has been completed. The steam lines have been reassembled, heat traced, and insulated.

The pressure vessel complete with circulator support spool pieces was subjected to leak tests and pneumatic pressure tests. These tests were witnessed by a Hartford inspector.

Insulation of the pressure vessel was completed during the period.

The helium circulator was stripped down and work in preparation for the steam run was completed.

The compressor wheel, steam turbine wheel, and Pelton wheel were balanced and subjected to spin tests. Each of these components has been spun at

14,000 rpm. The components were then inspected and shipped to Valmont.

The circulator has been reassembled in preparation for the steam/helium tests, which are scheduled to start early in the next quarter.

References

1. Ng, C. W., and Pan, C. H. T., "Linearized Turbulent Lubrication Theory," J. Basic Eng., Vol. 87, Issue 3, September 1965, p. 675; also ASME-Paper 64-LUB-29.
2. Burton, R. A., and Carper, H. J., "An Experimental Study of Annular Flow With Applications in Turbulent Film Lubrication," J. Lub. Technol., July 1967.

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.

Phase II of the test program was started during the latter part of the previous quarter. This is an environmental test in which the complete control and orificing assembly (control rod drive, orifice control mechanism and valve, and reserve shutdown system) are operated through a series of cycles in a helium atmosphere.

Operating temperature conditions are simulated in the autoclave; these range from 1200°F in the dummy core to 200°F at the control rod drive mechanism.

Figure III.1 shows the control and orificing assembly in the foreground prior to installation in the autoclave to the rear. This assembly weighs approximately 5000 lb and has an over-all length of 24 ft. Figure III.2 shows the control rod drive mechanism.

At the start of the test, the oxidant content of the gas was below the required 10 ppmv. The moisture content was approximately 20 ppmv. This, however, reduced to 1 ppm later in the test.

Core misalignment tests were performed with various displacements up to 1-5/8 in. These tests were performed at operating temperature conditions; no indication of increased friction or other erratic operation of the drive was observed.

Following a short period of shim tests, the motor brake coil failed, and testing was discontinued. An investigation showed the brake coil to have an open circuit. A new coil was wound under close supervision and given a preinstallation checkout for 20 hr in helium at 200°F, during which 900 switching cycles were performed with no evidence of coil deterioration.

High spots observed on the motor brake disks indicated inadequate seating, and prompted a run-in operation. After driving the energized brake (forcing the plates to slip) at 500 rpm for 4 min, 90% seating was obtained. The drive was reassembled, and testing resumed. On the twentieth shim operation, the rod position indicator ceased to track. Erratic behavior of the motor current and inability of the drive to scram upon release of the motor brake necessitated a second shutdown; the drive was operable, however, since the rods could still be inserted and withdrawn from the core.

Fig. III.1--Control and orificing assembly in the foreground prior to installation in the autoclave in the rear

Fig. III.2--Control rod drive mechanism

The innermost bearing supporting the clutch shaft was found to have failed due to the ingress of metallic and solid film debris. Figure III.3 indicates the extent of the debris adhering to the raceways of the bearing. The bearing was magnetized by the scram clutch coil located at the opposite end of the shaft. This clutch is part of a fast scram feature included for test purposes only and is not part of the PSC reference design.

The inner race of the bearing had fractured in two places subsequent to ingress of the debris and a chip had fallen out (Fig. III.4). This chip caused secondary damage, part of which was to the position transmitter drive gear, thus accounting for the malfunction of the position readout system observed at the time of failure.

There were no apparent metallurgical defects in the bearing, and no overload condition was found when the stress calculations were reviewed.

It was concluded that the debris resulted from an interference between the transmitter drive gear and the outer race of the bearing causing abrasion at that point. Dry film debris was produced by adjacent gearing.

As a result of general disassembly of bearings following this failure, it became apparent that some improvements were possible in the design of the bearing retainers.

It was observed that a register locating the halves of the bearing retainers (introduced to facilitate fabrication) caused the balls to ride against a sharp edge. The result was an effective scraper that removed the solid film from the balls which they normally transfer from the retainer to the raceways. Also, due to the minimal contact area presented to the ball, a rapid breakdown of the solid film lubricant resulted at that point.

The register was eliminated, and the split line was offset from the ball center, thus presenting an uninterrupted surface to the balls. On the basis of better antigalling characteristics and harder surface conditions, it was further decided to change the original K-Monel retainer material to 17-4 PH condition H900. Both of these materials have been used and tested on other applications. All bearings were replaced with new bearings incorporating the redesigned retainer.

The clutch shaft bearing was "insulated" from the shaft by the introduction of a nonmagnetic sleeve. Similar action was taken in the case of the shim motor bearings. As a further precaution against the ingress of debris into the clutch shaft bearing, a closure plate was fitted over its open end.

A bench test was performed on the scram clutch to determine whether any deterioration had taken place, since some increase in current had been noted during operation. After a 45-min energization of the clutch in air, the current showed a marked increase in excess of 300 ma, and the resistance a proportional decrease to 210 ohms. These values indicated an internal short in the windings.

Fig. III.3--Debris adhering to raceways of the bearing

Fig. III.4--Chip from inner race of the bearing, after fracture

As a result of this second coil failure, both the brake and the clutch coils were rewound, again under closely supervised conditions. This time, however, the coils were wound on spools fabricated from the same EPON 828 epoxy resin with which the coils are subsequently impregnated and encapsulated. This produces a better-wound, more reliable coil.

Reassembly of the control rod drive mechanism has been started.

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.

GAVEL - FULL-SIZE TUBE BUNDLE VIBRATION TEST

Fabrication of the Sulzer GAVEL tube bundle flow vibration test facility is complete, and preliminary tests have been made. A schematic of the test loop is shown in Fig. IV.1. A gas bypassing problem and a vibration induced by the blower were observed during preliminary tests. The amount of initial gas bypass was reduced to acceptable levels and the vibration has been eliminated. Testing began in accordance with the program upon correction of these problems.

Considerable data have been recorded during the GAVEL tests at various flow rates up to 135% volumetric flow. Recordings were made at incremental increases in blower rpm. Blower speed was increased in steps of 50 rpm from 1200 to 1450 rpm. At the lower flow rates (same as lower rpm rates), a perforated plate was installed in the vibration test rig to provide uniformly distributed flow through the test bundle. Data are being recorded on an eight-channel magnetic tape and by the Visicorder traces of the strain gages and the hot-wire anemometer output.

This testing program is proceeding at Sulzer. The analysis of the Visicorder charts from the testing is under way. A computer program is being written to convert the large amount of data into lift coefficient form.

In general, the test results indicate that there are no resonant conditions in the tubes; vibratory stresses in the tubes are less than ± 500 psi, which is approximately 1/10 of the predicted value; vortex shedding has been random; and acoustic coupling with radial-circumferential waves did not occur. Figs. IV.2 and IV.3 are graphs of mechanical vibration frequencies for various tube helix diameters. The tubes were excited by an electromechanical device in the test, and the graphs are plotted for vibrations in both the radial and longitudinal directions with respect to the tube bundle orientation. No air was flowing during these tests.

Fig. IV.1--Test loop schematic

Fig. IV.2--Tube frequencies - radial vibration

Fig. IV.3--Tube frequencies - longitudinal vibration

Lead-in and lead-out testing is continuing. The measurement of radial-circumferential wave frequencies, and Helmholtz-type wave coupling with vortex shedding remains to be done.

Lift Coefficient Test

An irregular in-line bundle with one instrumented tube was tested by Sulzer during the lift coefficient test. Preliminary results indicate a lift coefficient of 0.08 to 0.16. This coefficient value is approximately 3 to 5 times lower than the previous values obtained from the testing of uniformly staggered finned tubes and generally confirms the low-amplitude vibratory stresses observed in the GAVEL tests.

Tube Restraint - Thermal Contact Test

Testing was completed on four tube restraint designs and one welded tube configuration. Review of the data indicates that the thermocouples should be placed closer together around the tube-to-tube support joint. The results are inconclusive without further testing. Further testing will be performed with the tube restraint design selected for the PSC steam generator tube restraint systems. The results will provide information that will enable the thermal stresses in the tube-to-tube-support-plate joint to be more accurately evaluated.

Tube Restraint - Self-welding Test

The tube restraint testing program has continued. A variety of suggested sleeve materials have been studied for application as the protection sleeve between tube and support plate:

1. Plating Materials

- Chrome
- Silver
- Copper

2. Sintered Materials

- Bronze alloy
- Iron-carbon

3. Flame Spray Coatings

- Aluminum oxide
- Nickel alumide
- Chrome carbide

4. Solid Sleeve Materials

- Inconel 718
- Copper
- Cast iron
- TD nickel
- Stellite 6B

Test results to date indicate the following:

1. Of the plating materials, silver plating performed the best; however, some plating transferred from the sleeve to the tube. It remains unresolved if a sufficient amount of the protection layer will remain for a life-time system.
2. The sintered materials studied were apparently too brittle to withstand the vibratory joint action, and cracked during testing.
3. Aluminum oxide was the best performer of the flame spray coatings studied. It did, however, abrade the tube surface to a degree. Some chipping was noted on all of the spray coatings.
4. Inconel 718 and Stellite 6B were the best performers of the solid sleeve materials with Stellite 6B the better of the two. In most cases, however, some signs of surface damage were noted on the tubes.

In addition to materials investigations, sleeve designs were studied. The sleeves are intended to provide protection for the tube in the joint between the tube and tube support plates. These sleeves are designed to fill the 1/16-in. gap between the tube and the tube support plate. These designs included slit sleeve with a plate crimp, slit sleeve plus wedge, contoured hole, and a tapered hole-wedge combination. The latter design was the best performer. Testing is continuing on various combinations of designs and materials.

Consulting work has been in progress at Southwest Research Institute on the joint program. The material-damping properties of both the iron and Incoloy 800 tubes at steam generator operating temperatures were studied, and investigations into joint damping were pursued.

STEAM-WATER FLOW STABILITY TEST PROGRAM

Fabrication of the loop components and assembly of the boiling stability test loop have been completed. Figs IV.4 through IV.8 are photographs of various component parts of the test loop. The loop operational checkout has been started and a few items of equipment had to be corrected and adjusted. All major items, except for the combustor, are now operable including the canned rotor-water circulating pump, gas-fired preheater, charging pump, main air compressor, and plenum water level sensing device. Water-fill and degassing procedures were developed. Numerous dumps and fills were required to improve the water chemistry conditions so that the checkout runs could be made with heating. However, the solids content is still in the specified ppm range.

Stability analysis is under way with the DYNAM code using parameters describing the test section coil. Effort on the inclusion of a gas mixing subroutine into the HEAT computer program continued.

Fig. IV.4--External view of boiling stability test loop

Fig. IV.5--Test loop plenum, demineralizer, accumulator, and control valves

Fig. IV.6--Test loop chem pump circulating pump, condenser control valve, and air cooled condenser

Fig. IV.7--Test loop sample system, preheater, air silencer, and ducting to combustor inlet

Fig. IV.8--Test loop instrument and control panel

STEAM GENERATOR TUBE PLUGGING MACHINE

The final design layout for the support columns and the handling and installation equipment was completed. The construction drawings have also been completed except for checking.

This handling and installation equipment is mounted on a carriage that transports the equipment from the storage area to the steam generator module being repaired with the aid of the helium circulator handling carriage. All instrumentation and controls required for operation of the tube plugging machine are mounted on this carriage.

A hydraulic ram is used to insert the tube plugging machine into the reheat pipe after the reheat inner sleeve has been removed.

Four columns, each 11 ft long, are used to lift the tube plugging machine into position. A hand-operated jackscrew assembly is used for the final positioning of the tube plugging machine against the underside of the reheat seal. The entire assembly is spring-loaded against the underside of the reheat seal to minimize the problem of differential thermal growth between the reheat pipe and the tube plugging machine.

Final design layouts for the stationary housing and torque positioning system were completed, and preparation of the construction drawings for this equipment has been initiated. The mechanical design of the machine has advanced to a point where the electrical controls can be selected. Stepping motors and servo position-readout equipment have been selected. This equipment is capable of incremental movements of 0.001 in. in both vertical and circumferential modes. The azimuth drive consists of a stepping motor that drives through a 100-to-1 speed reducer to the azimuth drive gear. An anti-backlash mechanism has also been installed to eliminate a cumulative error through backlash. Position indication is detected through a ten-turn potentiometer. The vertical drive consists of a stepping motor with a synchro-transmitter output for position indication. The stepping motor drives a ball screw for raising and lowering the assembly.

Design of the tube plugging turret position sensing mechanism is in progress. This mechanism incorporates the use of eight SR-4 strain gages to sense the eccentricity of the turret alignment both in the vertical and azimuth direction with respect to the center of a steam generator tube.

A conceptual design layout for the reheat pipe seal-weld tools was also completed. The radiographic inspection head originally included in the design was deleted. The possibility of adding the radiograph inspection head will be maintained but no further design action is planned on this item. Final design and analysis of the remaining seal-weld equipment is continuing. The seal-weld tool consists of a standard welding head, a cutting head, a television viewing system, and a centering mechanism. The centering mechanism consists of rollers that are extended outward by the use of pneumatically operated cylinders. The cutting tool consists of a standard air motor and saw blade.

The saw cut, originally planned to be made at the centerline of the seal-weld, will be made above the centerline. The reason for the change was to make future joints easier to assemble.

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.

ISOTROPIC COATINGS

Determination of Preferred Orientation in PyC (Bacon Anisotropy Factor)

The difficulties encountered in determining the BAF on isotropic pyrocarbon coatings have been discussed in previous quarterly reports. The behavior of such coatings in an irradiation environment strongly suggests that the more nearly isotropic the structure, the better will be the performance of the coating. The present target is a BAF value between 1.00 and 1.10, and the lower the value, the better. This is a narrow range in which to see distinctions.

One method has been to run two different determinations on the same sample by the same and different operators and thus determine the reproducibility (see Table V.1). Because of the many tedious steps in the process, it is sometimes difficult to state with certainty where the takeoff point for the separate determinations began, but the most often used procedure was to use the same strip from one disk, and make two X-ray films of this one disk; one film was then carried through the procedure by one operator, and the second film by the second operator. In addition, the first operator calculated the BAF using two different methods. The first method is a precise technique that involves the plotting of a curve to fit the data points prior to the final calculation; the second method uses the short cut of not plotting the curve, but using the raw data points for the calculation. The second operator used the second method only.

The generalization that can be made at this time is that there is little difference between the two methods, but that there is a much larger difference between operators. Additional measurements are being made, and additional techniques are being developed.

Surface Porosity in PyC Coatings

There is evidence that the outer surfaces of LTI coatings have more open porosity than do HTI coatings. This is indicated in Fig. V.1, which is a

Table V.1
BAF DETERMINATION BY TWO METHODS AND TWO OPERATORS

Sample No.	Support Work for Capsule P19	First Operator			Second Operator		Difference Between Operators
		BAF		Curve Minus Points	BAF By Points	Density (g/cm ³)	Curve Minus Points
		By Curve	By Points				
4618	-	1.09	1.06	0.03	---	---	---
4622	-	1.08	1.08	0.00	---	---	---
4623	-	1.03	1.03	0.00	---	---	---
4624	-	1.01	0.99	0.02	---	---	---
4625	-	1.10	1.08	0.02	---	---	---
4626	-	1.08	1.08	0.00	---	---	---
4627	-	1.08	1.07	0.01	---	---	---
4628	-	1.12	1.09	0.03	---	---	---
4632	X	1.03	1.02	0.01	1.03	2.01	0.00
4636 ^a	X	1.02	1.02	0.00	1.09	1.89	-0.07
4636 ^a	X	1.02	1.02	0.00	---	---	---
4637	X	1.03	1.02	0.01	1.04	1.88	-0.01
4641	-	1.22	1.21	0.01	1.19	---	0.03
4642	X	---	---	---	1.05	1.82	---
4643	X	---	---	---	1.08	1.78	---
4644	X	1.04	1.00	0.04	1.03	2.01	0.01
4647	X	1.17	1.18	-0.01	1.11	1.90	0.06
4649	X	---	---	---	---	2.01	---
4650	X	1.09	1.10	-0.01	1.00	1.89	0.09
4652	X	1.09	1.08	0.01	1.04	1.74	0.05
4653	X	1.09	1.07	0.02	1.06	1.90	0.03
4654	X	---	---	---	1.09	1.74	---
4655	X	1.09	1.07	0.02	1.04	1.91	0.05
4658	X	1.08	1.07	0.01	1.06	1.91	0.02
4659	X	1.11	1.12	-0.01	1.03	1.80	0.08
4660	X	1.03	1.02	0.01	1.02	1.66	0.01
3592-53-E	-	1.23	1.23	0.00	---	---	---
3943-23-N	-	1.20	1.17	0.03	---	---	---
3663-53-E	-	1.07	1.07	0.00	---	---	---

^a Two determinations made on separate portions of same sample.

Fig. V.1--Comparison of porous volume in HTI and LTI coatings on P17 and P18 capsule material

plot of data taken with the mercury porosimeter apparatus. A sample of particles is placed in a mercury environment, and then the mercury is pressurized. Open porosity in the sample allows the mercury to intrude the coating. The volume of mercury intruded is a function of the applied pressure, and this is related to a pore diameter through the wetting angle of the mercury on the sample. Figure V.1 indicates that three LTI coatings show an increase in accessible volume at pore diameters in the range of 0.1 to 0.3 μ , whereas three HTI coatings show no increase in accessible volume over the entire range of the equipment (pore size equivalent to 0.085 μ). High-magnification pictures of the coating surfaces indicate this trend; Fig. V.2 is a comparison of an LTI coating with an HTI coating from the samples in Fig. V.1.

COATING EVALUATION

Thermal Stability

A long-term thermal stability test of BISO-coated particles has reached 3900 hr. These particles are at 1400°C under 2-psig helium. The condition of the samples after the examination at 2400 hr is shown in Table V.2. There has been no additional migration in the last 1200 hr. The surface contamination on one sample has increased by a factor of four; this does not detract from the results of the thermal stability study.

A summary of the three thermal stability tests on BISO-coated particles in which there has been activity during the last year is in Table V.3. Test No. 1, which was on three batches of BISO-coated particles, was conducted at 1400°C and was terminated after 9300 hr. All three batches showed good thermal stability. Test No. 2, which is presently in progress, is the one mentioned in the above paragraph. It contains a sample from one batch (3023-77E), which had seen the 9300-hr exposure in test No. 1. Test No. 3 was a 1-hr test at 2000°C on three batches of BISO-coated particles; all three batches showed good thermal stability with no cracked coatings. These three tests are summarized in Table V.3. Table V.4, which is a summary of the thermal stability tests performed on fuel particles that include metal carbide coatings, provides additional information on uranium migration into buffer coatings.

Crushing Strength of Particle Coatings

A knowledge of the ability of BISO-coated particles to resist a crushing force is of particular interest when the fuel particles are dispersed in a carbon matrix. This information is also of importance when applied to the ability of the particle to resist the internal forces due to the accumulation of gaseous fission products.

An experiment was conducted to measure the force required to crush individual particles. Since the particle is considered a hollow spherical pressure vessel because of the low strength of the buffer layer, a variety of sizes of particles were evaluated. The particles selected were from 2814-149E. This is a representative fissile particle batch that has been irradiated in three recent P-capsules. The method consisted of loading the individual particles in a load cell of a miniature Instron testing machine and applying a force. Breakage was indicated by a sharp drop in measured force.

Sample 3663-37-E

Sample 3663-25-E

Fig. V.2--Condition of the outer surface of LTI coating (left) and HTI coating (right). There appears to be more open porosity in the LTI coating. (1000X)

Table V.2
RESULTS OF BISO-HTI AND BISO-LTI LONG-TERM TEST AT 1400°C
(2400-hr Exposure)

Lot No.	Coating	Coating Thickness (μ)	Core Diameter (μ)	Test Time (hr)	Average Distance of Fuel Migration into Coating (μ)	Alpha Count (α/min-cm ²)
2814-149E	BISO (2200) ρ=2.07 g/cm ³	101	180	As coated	4	68
				600	7	(a)
				1200	6	563
				2400	5	582
3023-77E	BISO (2000) ρ=1.91 g/cm ³	97	207	As coated	0	16
				9300 ^b	2	57
				9900	2	(a)
				10,500	3	507
				11,700	3	597
3363-77E	BISO (1300) ρ=2.08 g/cm ³	94	169	As coated	0	9
				600	0	(a)
				1200	0	508
				2400	0	2166

^a Not determined.

^b Continuation of previous test at 1400°C.

Table V.2
RESULTS OF BISO-HTI AND BISO-LTI LONG-TERM TEST AT 1400°C
(2400-hr Exposure)

Lot No.	Coating	Coating Thickness (μ)	Core Diameter (μ)	Test Time (hr)	Average Distance of Fuel Migration into Coating (μ)	Alpha Count (α/min-cm ²)
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				9900	2	(a)
				10,500	3	507
				11,700	3	597
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				600	0	(a)
				1200	0	508
				2400	0	2166

^a Not determined.

^b Continuation of previous test at 1400°C.

Table V.3
THERMAL STABILITY TESTS ON BISO-COATED PARTICLES

Test No.	Batch No.	Temp (°C)	Time (hr)	Conclusions
1	{ 3023-77E } { 3023-83E } { 3031-37E }	1400	5000	Good thermal stability; 1, 11, 10 μ migration on batches as listed
			9300	No change from 5000-hr report
2	{ 2814-149E } { 3023-77E ^a } { 3363-77E }	1400	600	3363-77E is contaminated on surface; 3, 2, 0 μ migration on batches as listed
			1200	Additional contamination; no additional migration
			2400	Additional contamination; no additional migration
3	{ 3363-77E } { 3363-79E } { 2814-149E }	2000	1	No migration; no cracked coatings

^a
The particles in this sample have previously seen 9300 hr at 1400°C in Test No. 1; therefore, the times at temperature for this one sample are 9300 plus the time in hours.

Table V.4
THERMAL STABILITY TESTS ON METAL CARBIDE COATED PARTICLES

Batch No.	Temp (°C)	Time (hr)	Conclusions
3332-105 3332-127	2300	4	Survived well Very poor (survived at 1850°C, 25 hr)
3332-105 3332-127 3332-47 3332-49 3332-51	1850	25	Survived well Survived well Survived well Survived well Very poor, no PyC overcoat
3332-47 3332-49 3332-51 3325-87 3325-89 3325-117 3332-139 3332-141 3332-149	1700	200	Survived well Survived well Very poor, no PyC overcoat No migration 13 μ migration No migration 8 μ migration No migration No migration
2107-31 1985-77 3325-87 3325-89 3325-117 3332-139 3332-141 3332-149	1400	18,000 6783	1 μ migration 16 μ migration No migration 35 μ migration No migration 12 μ migration No migration No migration

Unfortunately the thickness of the hard PyC coating of the individual particles was not determined by radiography because the plates showed only the total coating thickness and core diameter. For the evaluation, it was necessary to assume a buffer thickness of 25 μ for all particles tested. A later radiograph showed that this is not valid, because there is a wide spread in the thickness of the buffer coating.

The results of the experiment are plotted in Fig. V.3. The PyC thickness used was the minimum measured total thickness minus the assumed 25- μ buffer thickness. Breakage was assumed to occur at point of minimum coating thickness. The slope of the curve indicates that a force of 16 g/ μ of coating thickness is necessary to break the coatings in the thickness range of 50 to 100 μ . Extrapolation of the curve passes through the origin.

DEVELOPMENT OF ALTERNATIVE TECHNIQUES FOR ESTIMATING CRYSTALLITE ORIENTATIONS IN ISOTROPIC PyC

The proper irradiation performance of BISO-coated HTGR fuel is strongly dependent on the degree of preferred crystallite orientation within the nominally isotropic pyrolytic coating. The present technique for measuring preferred orientation is based on the method described by Bacon (Ref. 1). This method uses X-ray diffraction techniques to determine the density of 0002 layer plane normals per unit solid angle from the normal to the coating surface. From knowledge of the distribution of plane normals the thermal expansion coefficients can be calculated for the direction normal to the PyC surface and in the plane parallel to the surface. The ratio of two thermal expansion coefficients is referred to as the Bacon Anisotropy Factor (BAF). For a perfectly isotropic PyC, the BAF would be 1.00. For highly anisotropic material, the BAF approaches infinity (thermal expansion in the basal planes approaches zero).

The BAF desired for PyC coatings on HTGR fuel is in a relatively narrow range of about 1.00 to 1.10. Therefore, precision in measurement is extremely important, and considerable effort has gone into development of a reliable BAF measuring technique. With the present method, a carbon disk about 1/4-in. in diameter by 1/16-in. thick is coated, along with fuel particles and the BAF is determined from strips of coating removed from the disk. Disks rather than fuel particles are used for the PyC source in order to obtain a large enough volume of material for X-ray diffraction. (The assumption is made that the BAF of the disk is the same as that of the particle coating.)

When carefully conducted, the present technique yields accurate BAF values. However, the length of time (about 4 hr) required to make a precise determination and difficulties arising from differences in disk and particle coating under certain circumstances have hampered the coating development effort. The time required for BAF determination would also represent a significant cost in control of quality of HTGR fuel in commercial production.

In view of the problems associated with present BAF determinations, a program has been initiated to develop alternative techniques for determining crystallite orientation in PyC coatings. An acceptable alternative technique

Fig. V.3--Crushing force on coatings of UC_2 fuel particles, batch 2814-149E

should reduce the cost and time required for estimating orientation of the PyC without sacrificing a significant amount of accuracy obtained with the present technique.

The aim of this study will be to develop a technique to correlate an easily measured property of PyC with the BAF. Thus, in an indirect manner the BAF of the sample may be estimated. A weakness in any method which attempts to determine crystallite orientation by techniques other than X-ray is that most material properties which are sensitive to crystallite orientation are also sensitive to characteristics such as porosity, density, crystallite size, and impurity content. For this reason, considerable experimental work will be needed for a successful method to provide a statistically convincing and reliable alternative to the current BAF measuring technique.

Emphasis is being placed on an optical technique utilizing the Maltese cross formation to characterize PyC. This optical feature is probably less structure-sensitive than other properties which could be measured and offers the most promise of success.

When viewed under the plane polarized light of a polarizing microscope the cross section of an anisotropic PyC coating on a spherical particle will display a pattern referred to as the Maltese cross. This optical feature is produced by successive maxima and minima of light intensity at 90° intervals around the spherical particle. A probable explanation of the phenomenon has been proposed by Gray and Cathcart (Ref. 2). They suggest that when plane polarized light strikes the surfaces of PyC crystallites which are not perpendicular to the c-axis it is reflected in one of two possible modes. If the azimuth of polarization of incident light is parallel or perpendicular to the c-axis of the crystallites, the reflected light will be plane polarized in the same azimuth. Light reflected from these areas will be extinguished by the microscope analyzer and dark bands will be seen on the specimens in these regions. When the plane of polarization of incident light is not perpendicular or parallel to the c-axis the reflected light is elliptically polarized and a portion of it will pass through the analyzer of the microscope to produce light regions on the specimen image. Maximum brightness is observed along diameters inclined at 45° to the azimuth of polarization and maximum darkness along diameters including the azimuth and at 90° to it.

Recent work by Gulf General Atomic reported in the previous quarterly report has attempted to correlate BAF and the intensity of the Maltese cross observed in particle coatings. One conclusion from that work was that the cross may be observed in fuel particle coatings with BAF as low as 1.08 when the coating was applied at high temperature (about 1800°C). However, when coatings were deposited at low temperature (about 1300°C) the cross was not discernible at BAF values lower than 1.17. There are at least three possible explanations for this result:

1. The degree of preferred orientation at which the cross becomes evident is sensitive to a significant extent to properties other than crystallite orientation.

2. The error involved in those particular BAF determinations may be large enough to account for the apparent difference in BAF at onset of Maltese cross formation.
3. Although an attempt was made to prepare and photograph all specimens using identical metallographic techniques, slight variations may have produced the observed results.

It was also observed that varying photographic exposure time from 1/2 to 4 sec caused no visual difference in the Maltese cross. There was, however, a significant increase in the visibility of the Maltese cross as magnification of the photograph decreased from 200X to 50X.

A program has been planned which will extend the previous work in an attempt to establish more precisely the nature of the relationship between BAF measured on disk coatings and the Maltese cross formation in fuel particle coatings. Of particular interest will be correlation in the BAF range of 1.00 to 1.20.

Coated fuel particles from both high- and low-temperature coating runs with a range of BAF from 1.00 to 2.20 will be selected from batches with known BAF. These particles will be prepared by a standard metallographic technique in which variables are minimized and cross formation is enhanced. When possible, disks from respective coating batches will also be prepared. Standard photographic techniques will be developed by variation of exposure time, and the intensity, incidence angle, and wave length of polarized light to give enhanced Maltese cross formation.

Coated particles will be observed and photographed under polarized light. A mosaic of photographs of coatings with BAF values ranging from 1.00 to 2.20 will be prepared. This mosaic will serve as a standard for visual comparison with photographs of particles with unknown BAF prepared by the standard techniques. An estimate of BAF of the particle coating should then be possible.

An effort will also be devoted to quantitative measurement of the intensity of the Maltese cross. In this work a negative of a photograph of a coated particle will be scanned by a densitometer. This will provide a measure of light reflected from the PyC as a function of angle from the azimuth of polarization of incident light. A densitometer designed for this purpose will be fabricated to conduct this portion of the work. Standards will be prepared using PyC of known BAF. Comparison of sensitivity and reliability of visual and instrumented grading of optical features will be made.

The program was initiated during the latter part of this report period. A large number of batches of coated enriched fuel particles for which the BAF has been accurately determined were on hand, and samples of batches representing a range of BAF from 1.00 to 2.20 were selected for the study. Fabrication of a special densitometer for scanning photographs was begun.

If correlation of BAF with optical features of PyC does not prove feasible, magnetic susceptibility will be investigated. Strips of PyC hung between strong magnets may orient in a preferred direction. The tendency for preferred orientation would probably increase with increasing BAF values.

Review of the literature will continue during the program. New techniques which are uncovered by this effort and which appear promising will be investigated.

References

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2. Gray, R. J., and J. V. Cathcart, "Polarized Light Microscopy of Pyrolytic Carbon Deposits," J. Nucl. Mat., Vol. 19, 1966, pp. 81-89.