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

AEC RESEARCH AND
DEVELOPMENT REPORT

GA-8725

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

QUARTERLY PROGRESS REPORT
FOR THE PERIOD ENDING
JUNE 30, 1968

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

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QUARTERLY REPORT SERIES

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GA-7453-July, 1966, through September, 1966
GA-7634-October, 1966, through December, 1966
GA-7939-January, 1967, through March, 1967
GA-8038-April, 1967, through June, 1967
GA-8270-July, 1967, through September, 1967
GA-8420-October, 1967, through December, 1967
GA-8600-January, 1968, through March, 1968

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 585-psig (NWP) sustained pressure test at ambient temperature, which was resumed on January 30, 1968 after the completion of the detensioning tests (see the previous quarterly report, GA-8600), is still in progress. Data sampling has continued at the rate of two per week. Data evaluation for creep effects has been made difficult, because of the seasonal variation in the ambient temperature. The observed creep deformations must be corrected for temperature effects before a meaningful evaluation of creep due to internal pressure and residual prestress can be made. The model has been maintaining the internal pressure within ± 20 psig, the pressure variation being attributed primarily to temperature changes.

PCRV Model No. 2 Design and Analysis

In conjunction with the investigation into the concrete temperatures during cement heat of hydration of the preplaced aggregate concrete bottom head of the Fort St. Vrain PCRV, a thermal analysis was made on Model No. 2 for the purpose of comparing the analytical results with measured concrete temperatures. The analysis included a two-dimensional thermal model and concrete thermal properties obtained from tests conducted at the University of California at Berkeley (see "Concrete Properties" below).

Figure 1.1 shows the thermal model and the location of the different thermocouples whose places and numbers are identical with the test model. Figure 1.2 shows the comparison graphically for two extreme locations in the

* INDICATE POSITION AND IDENTIFICATION NUMBER OF CONTROL POINTS.

Fig. 1.1--Thermal model of the bottom head of Model 2, R-Z geometry

Fig. 1.2--Comparison between calculated temperatures and measured data for two locations in the concrete (PCRV Model No. 2). The numbers of the curves are identical with the thermoelement number

concrete. Element 307 represents a point less dependent on the outside boundary conditions, whereas element 357 is near the outer surface and is thus very dependent on the heat transfer to the surroundings. It is seen that the calculated temperatures compare favorably with the measured values during the initial period of temperature rise. The calculated maximum temperatures are higher than the measured results and the Model No. 2 temperatures also decrease more rapidly. Lower temperatures for the model mean (1) a lower heat of hydration of the actual cement used, (2) greater heat removed by the inside steel, or (3) higher losses to the surroundings. It should be emphasized that the analysis was based on radiation and free convection only. No estimates were made to check the effect of forced convection (wind).

Prestressing System

A full-size thermalized wire curved tendon for the long-term relaxation test at 120°F was stressed and anchored at 1400 kips (0.7 GUTS). This tendon is being tested in a 16-ft-radius semicircular tube. No-Ox-Id-Cm casing filler was used as lubricant during tendon fabrication and tendon installation. The relaxation loss recorded after 500 hr is approximately 1.7% at an average temperature of 120°F.

The two straight 25-wire (thermalized) tendon relaxation tests have completed more than 4000 hr. One tendon is being tested at 120°F and the other at ambient temperature. The relaxation losses after 4000 hr indicated no significant change from the relaxation measurements taken after 2000 hr which are shown on Fig. 1.1 of the previous quarterly report (GA-8600). The indicated relaxation losses after 4000 hr are approximately 3.1% at 120°F and 0.75% at ambient temperature (approximately 70°F).

The 1000-ton tendon anchor test beam was prestressed during the quarter. The tendon was initially stressed to 1700 kips (0.85 GUTS), held for about five min, and then reduced and anchored at 1400 kips (0.7 GUTS). After about four weeks under load, the test beam was heated at a rate of approximately 15°F per day to a maximum concrete temperature of 130°F.

Figure 1.3 shows a typical stress-strain curve for 1/4-in. diameter thermalized wire manufactured by Richard Johnson and Nephew, Ltd. Similar wires are being used to fabricate the full-size tendon for the prestressing system.

Concrete Properties

Phase II Tests. Triaxial creep tests on two sealed specimens (7-in. cubes) at room temperature are in progress. The initial hydrostatic pressure of 1600 psi was maintained for 62 days, after which the pressure was increased to 2250 psi (maximum limit of the present hydrostatic chamber test setup). No appreciable creep strains were observed when the specimens were subjected to 1600-psi triaxial loading; the total creep after about 60 days under load was within 30 μ in./in. No definite creep trend has been observed thus far on the 2250-psi triaxial loading.

Fig. 1.3--Stress-strain curve for straight thermalized wire 1/4 in. diam by 24 in. long

The premature failures on uniaxial creep cylinder specimens in the 150°F temperature environment that were discussed in the previous quarterly report have been definitely attributed to deterioration of the sulfur end cap. Additional 6- by 12-in. cylinders were placed under load in the 150°F water bath after the sulfur caps were replaced by cement-sand mortar caps with steel bearing plates and resealed.

Eight cylinder specimens are under applied stresses varying from 2250 to 3390 psi at 150°F and no failures have occurred, though previously loaded specimens with sulfur caps failed within hours when loaded to 3390 psi. Creep results of six uniaxial cylinders are shown in Fig. 1.4; the other two specimens were placed under load late in the quarter, and limited data are available at this time.

Results of the thermal and chemical compatibility studies of the concrete components (i.e., aggregate and cement) under the temperature and stress test conditions were completed. The results showed that the premature failures observed in the 150°F creep cylinders were not associated with any chemical action that may have caused concrete deterioration. The results of petrographic examination confirm the suitability of the crushed andesite aggregate for PCRV construction.

All creep specimens under Phase II of the test program have been placed under load. In addition to the uniaxial creep tests on 6- by 12-in. sealed concrete cylinders discussed above, four biaxial cruciforms are under load at an applied stress of 3210 psi, two specimens each in the 150°F and 74°F water baths.

Most of the shrinkage and creep results were reported in the previous quarterly report (GA-8600). No significant changes have been observed in the reported shrinkage and creep trends. Additional creep results from uniaxial cylinders loaded at 230 to 262 days of age are shown in Figs. 1.4 and 1.5. Figure 1.4 shows the creep behavior of specimens under load of 2250, 2820, and 3390 psi at 150°F. The nonlinear behavior of elevated temperature creep with time, which was first reported in GA-8600, is substantiated by the results shown in Fig. 1.4. Figure 1.5 gives additional creep curves of specimens under load of 2250, 2820, and 3390 psi at 74°F. A comparison of these curves with those of Fig. 1.2 (specimens loaded at 30 days of age) and Fig. 1.3 (specimens loaded at 100 days of age) of GA-8600 show the effect of concrete age at the time of loading.

Preplaced Aggregate Concrete Mix Test Program. Most of the program on preplaced aggregate concrete (PAC) at the University of California at Berkeley has been completed and the results have been received. Still to be received are results of strength tests on PAC cylinders at 180 days of age and additional heat of hydration results on the cement.

Results of the test program are summarized in Table 1.1. Two grout mixes consisting of cement, fly ash, and sand were investigated, 9:1:10 and 9:1:8, with sufficient water to produce a consistency (flow) factor of 22 sec. The properties of the grout mixes are given in Table 1.1. Strength test results of grouted PAC cylinders using both grout mixes are also given in Table 1.1. Results are tabulated for average compressive strength, tensile

Fig. 1.4--Uniaxial creep of specimens loaded at 230 to 262 days of age at 150°F, autogenous shrinkage included

Fig. 1.5--Uniaxial creep of specimens loaded at 230 to 262 days of age at 74°F, autogenous shrinkage included

Table 1.1

SUMMARY OF PROPERTIES OF GROUTS AND OF 6- BY 12-IN. PAC SPECIMENS
CONTAINING TYPE II, LOW-ALKALI, LOW-HEAT CEMENT

Properties	Mix 9:1:10		Mix 9:1:8	
Intrusion Grouts				
W/(C + FA), by weight	0.47		0.44	
Grout temp, °F	47		46	
Flow factor, sec	21.8		21.1	
Consistency factor, degrees	188		188	
Extraction factor, min:sec	4:21		4:38	
Unit weight of grout, lb/ft ³	129.7		128.8	
Test at ambient temperature	45°F	70°F	45°F	70°F
Expansion at 5 hr, %	7.6	11.7	8.6	12.5
Bleeding at 5 hr, %	0.8	0.8	0.6	0.5
Time of Set.(Vicat), hr:				
Initial	32½	16	32	17½
Final	35	18	35½	19
Preplaced Aggregate Concretes				
Type of curing at 70°F				
Compressive strength, psi:	Moist	Sealed	Moist	Sealed
7 days	2690	2470	2950	2950
28 days	5960	5610	6060	5560
90 days	7620	6760	7600	6890
180 days ^a				
Tensile splitting strength, psi:				
28 days	611	611	631	607
90 days	689	633	688	704
180 days ^a				
Mod. of Elast. E (10 ⁶ psi), and Poisson's ratio μ :	E	μ	E	μ
7 days	3.1	0.18	3.1	0.19
28 days	4.1	0.21	4.1	0.21
90 days	4.7	0.20	4.4	0.21
180 days				
Unit weight, lb/ft ³ , (avg. up to 90 days)	147.0		146.7	

^a Not available at present.

splitting strength, modulus of elasticity, and Poisson's ratio of moist-cured and sealed 6- by 12-in. cylinders tested at ages of 7, 28, and 90 days. The results of the test showed that either the 9:1:10 or the 9:1:8 grout mix with mixing water content sufficient to produce a grout flow factor of 26 ± 2 sec would meet the strength requirements and groutability requirements of the PSC for the PCR bottom head.

Values of the heat of hydration on a 9:1 mixture of cement and fly ash with a water to cement and fly ash ratio $W/(C + FA)$ of 0.44 as determined by the conduction calorimeter and by the heat of solution method are presented in Table 1.2. The heat of hydration of cement plus fly ash is well below the maximum usually permitted for Type II cement by ASTM C150. The heats of hydration for an average Type II cement as tested by the U. S. Bureau of Reclamation shown in Fig. 23 of the Concrete Manual (see Ref. 1)* are given in Table 1.2. Note that the USBR value for one day is more than double that of cement plus fly ash and nearly 50% greater at ages of three and seven days.

Table 1.2
HEATS OF HYDRATION^a

Age (Days)	Heat of Hyd. by Heat of Sol. (cal/g)	Heat of Hyd. by Cond. Calor. (cal/g)	Avg Type II ^b (cal/g)
1/2	--	4.6	--
1	--	20	42
2	--	37	--
3	42	44	64
4	--	48	--
5	--	51	--
7	58	--	75
14	68	--	--
23	70	--	--
28	--	--	83
30	71	--	--

^a See. Ref. 1.

^b Ref. 1, Fig. 23.

*References will be found at the end of each task.

Ten sealed PAC cylinders, 6 in. by 12 in., were obtained from the University of California at Berkeley. These were extra cylinders made during the PAC mix test program and employed 9:1:10 and 9:1:8 grout mixes. The cylinders were provided with cement-sand mortar caps with steel plates, and eight of them were loaded at an age of approximately 90 days to applied stress levels of 2820 and 3390 psi at 74° and 150°F. These tests will compare the long-term strength of the PAC with that of conventional concrete used in Phase II tests. The PAC cylinders were not instrumented.

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).

A series of tests was run to assess the ability of the Class A thermal barrier to withstand a sudden accidental depressurization of the PCRV. The worst accident condition results in a depressurization of the pressure vessel at a calculated initial rate of 40 psi/sec. Panels were subjected to this depressurization rate, with ambient helium, within a high-pressure autoclave test facility. Each panel was also subjected to a depressurization rate of 0.5 psi/sec (reactor load change and startup and shutdown pressurization rates).

The following insulation panels were tested:

1. Four 1-in. layers of Kaowool (8 lb/ft³) and one 1-in. layer of Microquartz (8 lb/ft³) compressed to a thickness of 3 in. (12 lb/ft³).
2. Eight 1/2-in. layers of Microquartz (8 lb/ft³) compressed to a thickness of 3 in. (12 lb/ft³).
3. A 3-in. thickness of metallic insulation consisting of twenty single gas layers.
4. A 4.5-in. double layer of Kaowool.

The specimens were housed in the fixture illustrated in Fig. 1.6. The fibrous insulation panel layout represents the intersection of six panels (see Fig. 1.7) rather than a single panel. This configuration permits testing of the weaker panel edges and seals. The equivalent peripheral volume, i.e., center to edge, of the six panels was calculated and added as a gas tank (Fig. 1.6). This reduced the specimen size, yet utilizes maximum autoclave space.

The 3-in.-thick fibrous material was installed in the test fixture very carefully to obtain correct density and layer uniformity. To obtain a good fixture edge contact, the fibrous material was cut 0.5-in. oversize and carefully installed, yielding a small amount of lateral compression. The stud holes were then made and the studs installed. Some of the studs were made

Fig. 1.6--Thermal barrier depressurization test on fibrous and metal specimens

especially to provide panel venting. The 20 gage overlapping seal plates were installed next to the fiber. The 0.25-in. steel cover plates were installed using clamps and weights to compress the fiber from 8 to 12 lb/ft³ density. The plates and seal sheets were secured to the test fixture edge with a neoprene rubber gas seal. The test fixture is a leaktight assembly, whose perimeter is a plenum chamber (edge manifold and gas tank). The manifold is connected through six 1-in. pipes to the gas tanks (see Fig. 1.6). The structural box was designed to withstand the depressurization pressures without distortion by utilizing both stiffeners and the gas tank for structural support.

The Class A metallic thermal barrier panel was mated to the test fixture with a metallic adapter frame that is designed to compensate the height differential and to mate with the vertical seal strips. The test fixture perimeter plenum was sealed off, and the gas tanks were vented during the metallic panel depressurization test. The metallic panel base (3/4-in. contoured liner plate) was bolted to the fixture. The seal between the liner and metallic panel was effected by a crushed strip of stainless steel honeycomb. The metallic panel consisted of a 1/4-in. steel front-back plate, with 20 interlayer sheets and spacers forming separate gas cavities (Fig. 1.7). The assembly is contained with spring-loaded swivel bolts to permit differential thermal expansion. The panel swivel bolts were finger tight, whereas the attachment bolts were torqued to 100 in.-lb. The specimen perimeter seal strips were mated to the adapter frame (Fig. 1.6). The metallic panel was tested with and without a vent orifice, which was located at the center of the panel (Fig. 1.16).

The double-layer fibrous design required another fixture modification. This modification required an additional peripheral gas manifold to be connected to the gas manifold and tank used in the single-layer fiber tests. This is accomplished by using alternate holes (for support and gas venting functions) in the basic test fixture. Specimen (Kaowool) assembly was again carefully controlled to obtain the correct density, a good fit at the fixture edges, and a leak-free edge seal at the metallic cover plates. The attachment studs were torqued down to obtain the correct fiber thickness for this installation.

During tests 1 through 4, measurements consisted of the autoclave gas pressure and temperature and before and after measurements of the specimen cover plate surface. During tests 5 through 10, additional instrumentation was added to assess surface motion and specimen internal gas pressures. During the final two tests, two calibrated load cells were attached to assess stud loads during depressurization.

After specimen and instrumentation assembly, the complete test fixture was installed in the high-pressure autoclave, which was slowly pressurized with helium. The autoclave was depressurized by using a hand valve rather than the rupture mechanism shown in Fig. 1.6, because it was easier to obtain reproducibility by this method.

The two conditions (reactor startup-shutdown and worst accident depressurization) were simulated in the test by depressurizing the specimen from

Fig. 1.7--Test panel geometry for rapid depressurization test

480 psi (at ambient temperature) at rates at least as high as the calculated conditions of 0.5 and 40.0 psi/sec.

Twelve tests were conducted. The surface deflections obtained through pre- and post- test measurements at the six locations identified on Fig. 1.8 are given in Table 1.3. All panels tested remained intact at depressurization rates in excess of 40 psi/sec, but some permanent deformation of the cover plates occurred after the higher rate tests. Typical before and after photographs are shown in Figs. 1.9 through 1.19.

The two-layer Kaowool panel was depressurized at the highest rate of all tests (62 psi/sec), and the specimen remained intact (Fig. 1.19).

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.

Modifications of the purified helium compressor motor, which failed during the previous quarter, were completed. The modifications included a new stator with additional insulation and other changes (see the previous quarterly report, GA-8600), modification of the rotor, and installation of a volt trap. The new stator, prior to final varnish dip and bake, was tested by the manufacturer for corona at 12.5 psia in a helium atmosphere. Corona occurred at 740 v (400 cycles) rms and 1050-v peak.

Testing of the compressor was resumed. The head-flow characteristic curve at 12.5-psia discharge pressure was determined and is shown in Fig. 1.20. Motor performance was normal and the cause of the previous failure has apparently been eliminated.

Ten start-stops were then performed. Performance of the compressor appeared to be satisfactory. An analysis made at Gulf General Atomic of a gas sample taken during the start-stop tests indicated that the test loop gas contained 2.1 ppm O₂, 30 ppm N₂, 5 ppm $\pm$ 5% H₂O, and less than 0.5 ppm each of CO, CO₂, and CH₄.

At the completion of a 100-hr test, the purifier cold trap was regenerated and the dessicant cartridge was replaced. Forty start-stops were then made, with gas samples taken at the beginning and the end, and the 900-hr test was started. The latter test is being run continuously and is expected to be completed June 20th.

The gas samples were analyzed at Gulf General Atomic, and the results are given in Table 1.4. Dew point readings made at MTI were about -96° to -98°F, indicating a moisture content of about 1 to 2 ppm. The latter may be more reliable, since moisture might outgas from the walls of the sampling bottles.

DIMENSIONS IN
INCHES

Fig. 1.8--Deflection measurement locations in rapid depressurization test

Table 1.3

RESULTS OF RAPID-DEPRESSURIZATION TESTS ON THERMAL BARRIER PANELS

Test No.	Decay Rate (psi/sec)	Specimen ^a	Vent	Deflection at Location Identified on Fig. 1.8 (in.)					
				1	2	3	4	5	6
1	1.2	1	Open	-0.006	+0.005	+0.002	+0.001	+0.001	0.000
2	1.2	1	Closed	+0.005	0.000	+0.006	+0.001	+0.006	+0.003
3	41	1	Open	+0.018	+0.017	+0.013	+0.033	+0.025	+0.027
4	43	1	Closed	+0.054	+0.041	+0.060	+0.064	+0.070	+0.078
5	0.83	2	Closed	-0.040	-0.041	-0.047	-0.035	-0.045	-0.042
6	45	2	Closed	+0.799	+0.772	+0.834	+0.783	+0.792	+0.758
7	1.2	3	1/4 in.	0.000	0.000	--	--	--	--
8	40	3	3/8 in.	+0.001	-0.009	--	--	--	--
9	40	3	5/16 in.	+0.001	-0.006	--	--	--	--
10	35	3	None	+0.006	-0.005	--	--	--	--
11	1.2	4	None	0.000	+0.016	-0.002	+0.013	-0.003	+0.002
12	62	4	None	+0.053	+0.059	+0.046	+0.034	+0.053	+0.042

^a

1. Kaowool and Microquartz.
2. Microquartz.
3. Metallic.
4. Double-layer Kaowool.

Fig. 1.9--Rapid depressurization test setup containing microquartz specimen before testing showing test fixture in high-pressure autoclave

Fig. 1.10--Rapid depressurization test setup containing Kaowool and micro-quartz after test; panel vents open, decay rate 33.0 psi/sec

Fig. 1.11--Rapid depressurization test setup containing Kaowool after test; panel vents closed, decay rate 33.0 psi/sec

Fig. 1.12 --Rapid depressurization test setup containing microquartz before test; panel vents closed, decay rate 0.5 psi/sec

Fig. 1.13--Rapid depressurization test setup containing microquartz after test; panel vents closed, decay rate >33.0 psi/sec

Fig. 1.14--Rapid depressurization test setup containing microquartz after test; panel vents closed, decay rate >33.0 psi/sec

Fig. 1.15--Rapid depressurization test setup containing metallic specimen before test; panel vent 3/8-in. orifice, decay rate 33.0 psi/sec

Fig. 1.16--Rapid depressurization test setup containing metallic specimen after test; panel vent closed, decay rate 33.0 psi/sec

Fig. 1.17--Rapid depressurization test setup containing metallic panel before test

Fig. 1.18--Rapid depressurization test setup containing two layers of Kaowool before test; panel vents closed, decay rate >40.0 psi/sec

Fig. 1.19--Rapid depressurization test setup containing two layers of Kaowool after test; panel vents closed, decay rate >40.0 psi/sec

Fig. 1.20--Purified helium compressor low-pressure performance

Table 1.4
RESULTS OF HELIUM SAMPLE ANALYSES

Impurities	Amount Detected (ppm)	
	Before	After
Ne or H ₂	~20	~20
Ar or O ₂	4	2
N ₂	6	10
CH ₄	N.D.	N.D.
CO	N.D.	N.D.
CO ₂	N.D.	N.D.
H ₂ O	9±5%	10.8±5%

The purity of the helium during the start-stops was not as high as it should have been. The purity is not of much concern during normal compressor operation when moving surfaces do not touch. The start-stops will be repeated with a higher purity helium.

INSTRUMENTATION AND ELECTRICAL

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

Fuel Element Thermometers

Thermal cycling of the four thermocouples has continued, with 12,300 total cycles being logged at the end of the reporting period without failure.

Two additional Geminol thermocouples fabricated to nuclear grade specifications were added in late May in order to improve the sensor lifetime prediction.

Moisture Detectors

Microballoon glass spheres of a high-temperature type will be used to help insulate the heater from the cold sink in the moisture detector.

The temperature cycling test of the coupling connecting the gaseous-nitrogen cooling coil of the detector to the inlet and outlet coolant lines

has continued. The testing was begun because a potential problem was identified at the joint between the copper tubing of the mirror cooling coil (operated near liquid-nitrogen temperature) and a stainless steel coupling. These components are located inside the penetration interspace. It was decided to thermally cycle the joint between -300° and $+150^{\circ}\text{F}$ at a rate of $150^{\circ}\text{F}/\text{min}$ 660 times, which is the expected number of cycles for one year's operation. At the end of about 500 cycles the tubing broke, but not at the joint at the fitting. An expansion in the form of tubing loop was incorporated and the test continued. It is now planned to cycle the joint (which has not failed) and the new tubing loop an additional 660 times. Of these, about 300 cycles have been completed during the reporting period. The fitting and tubing are subjected to an external pressure of 700 psig during testing to simulate the penetration environment.

FACTOR, the digital computer program to facilitate high-accuracy calibration of the mirror thermocouple, has been received from the National Bureau of Standards and a trial run has been executed successfully on the 1108 computer. It will be utilized to calibrate production models of the moisture detector.

Development work on the electronic circuits was completed, excepting the liquid-nitrogen control. The electronic circuits are now in package design and the trip module has been completed. Temperature testing of the individual electronic modules has also been completed. This completes the research and development phase of the electronic equipment.

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.

A television camera was placed in an autoclave and tested for performance at elevated temperature. The camera performance was evaluated by observing a test pattern that shows the number of lines the camera will resolve at a distance of 18 in. and the number of shades of gray the camera will discern. The camera specifications at ambient temperature require the camera to have 600 lines of horizontal resolution and 350 lines of vertical resolution with ten shades of gray. The standard television test pattern chart has a maximum horizontal and vertical resolution of 800 lines and ten shades of gray.

At room temperature at the beginning of the test, the camera had 800 lines of horizontal resolution and 600 lines of vertical resolution with ten shades of gray. At 210°F the picture quality had degraded to 600 lines of horizontal resolution and 400 lines of vertical resolution with seven shades of gray. The lack of three shades of gray gave the picture a washed-out appearance that would be functional for limited use.

At 215°F the picture went dead as a result of a short between the camera chassis and a Vidicon filament wire. The short was repaired and the camera was returned to the autoclave and prepared for the 48-hr holding test at elevated temperature.

A drawing of the test specimen for the reserve shutdown vacuum tool developmental test has been prepared from layout drawings and is now being reviewed. The test specimen is a mockup of the production vacuum tool that will evaluate the vacuum capabilities of the tool. The mechanical locating and guiding devices were not included in the developmental test specimen. The locating and guiding devices will be evaluated during the checkout and proof tests to be done at a later date.

The gas path and other parameters that would affect the suction capabilities have been included in the test specimen. The vacuuming test will be done at room temperature in helium and at a reduced pressure (9.5 psia) to simulate the density of the helium at 250°F. The reserve shutdown vacuum tool probe will be lowered into a transparent plastic tube that will simulate the reactor core and also permit observation of the vacuum operation.

RESERVE SHUTDOWN SYSTEM

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

A test program is under way to establish the fragmentation characteristics of graphite rupture disks and the free fall characteristics of granular absorber material from the storage hopper.

Drop tests were performed on a quantity of scored or "waffled" disks, the intent of the scoring being to cause fragmentation into pieces of consistent size that are at the same time small enough to be retrieved by the vacuum removal system. In the tests, the scored disks fragmented into large pie-shaped pieces (see Fig. 1.21), the breaking taking place along the radial score marks rather than the circumferential marks.

The column height formed by the fallen granules has generally exceeded the anticipated height by 7% to 8%. The dead-weight reading of the material at the bottom of the channel has consistently been about 5% of the total material weight. These results will be reviewed when the present series of tests has been completed, but the poison density in the fallen column has already been found acceptable for achieving the desired reactivity effects.

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.

Environmental testing of the orificing mechanism and valve has continued in conjunction with the control rod drive test program. Approximately 302,100 valve cycles (or steps) have been completed; this exceeds test specification requirements by approximately 90%. Cycling will continue for the duration of the control rod drive test, or until the redesigned mechanism and valve assembly are available.

Fig. 1.21--Fragmented rupture disk

CONTROL ROD AND SHOCK ABSORBER

The objective of this subtask is to develop a control rod assembly and a shock absorber that meet the requirements for the plant, and to demonstrate the mechanical performance of a mockup of the rod under abnormal conditions.

Drop tests were completed on the control rod and shock absorber components concluding this phase of the control rod research and development program. These tests were made to verify the design and operation of control rod and shock absorber components, and to demonstrate the capacity of the graphite base block that forms the bottom of the control rod channel to withstand the impact forces resulting from an accidental failure of the rod support cable or support spine.

Initial survey tests on the bellows type shock absorber were reported in an earlier quarterly report (GA-7937). The tests reported here include all work associated with this phase of the test program, including the results of the early survey tests.

A series of 17 shock absorbers was fabricated and tested in a drop rig to simulate the specified accident conditions. In the initial tests, the shock absorbers were attached to the lower end of the lead-filled simulated control rod; some tests were made with the shock absorber and two container can sections, and other drops were made with the shock absorber and a single container can section. The final test was made using a complete control rod which resulted in a peak impact force of 8500 lb (71-g deceleration) and a unit load of 4250 psi on the simulated graphite reflector block with no resultant damage.

The drop tests were performed in air, the shock absorber being heated to the desired test temperature just before the rod was released.

Table 1.5 describes the test condition and rod simulation configuration for each test.

The test rig consisted of a guide tube, release mechanism, shock absorber heater, and simulated lower reflector blocks. The lower 3 ft of the guide tube was fitted with a clear plastic section in order to view and film the shock absorber collapse.

The control rod was simulated by a lead-filled section of tubing that was adjusted to the total control rod weight. The lower end of the simulated rod was formed to the configuration of a neutron absorber can. For the tests that were performed with two neutron absorber cans, the weight of the lead-filled tube was reduced to maintain the proper rod simulation.

The impact force on the graphite block was measured with an accelerometer for the first six tests. The accelerometer was attached to the top of the lead-filled ballast tube and the signal recorded on an oscillograph. The arrangement was found to be inconvenient, because the instrument wires had to be attached to the suspension cable and tended to tangle during the drop. In the remaining eight tests, a 15,000-lb load cell was placed below the graphite blocks and the impact load recorded on an oscillograph.

The graphite blocks that simulated the lower reflector blocks were made of grade HC graphite in two configurations. One design has a 1/2-in. deep register for the plastic tube section and permitted filming of the shock absorber collapse. The second design consisted of two blocks, the top one containing coolant holes as well as a through hole for the control rod channel (see Fig. 1.22). The lower block was solid and was doweled to the top one. This second design simulated the corner of a reflector block and verified its structural integrity against any lateral load imposed by the collapsing shock absorber. The graphite blocks were supported by a steel plate (12 in. square by 1 in. thick) that was securely bolted to produce the maximum shock load on the graphite block.

The guide tube was a 29-ft steel tube that had an internal diameter equal to the reactor core channel. Sixteen feet from the bottom of the guide tube was a heater assembly that was used to preheat the shock absorber prior to a test. The surface temperature of the shock absorber was monitored with a thermocouple. The bottom of the guide tube was vented with four 1-in.-diameter holes to relieve the air when the rod was dropped.

The release mechanism consisted of a latch block with a safety pin and solenoid.

The initial four shock absorbers, which were tested with a simulated rod weight of 148 lb, were dropped from various heights at ambient temperature. The impact of each test was filmed by a 400 frame-per-second motion picture camera to document the collapse characteristics. Shock absorber samples 5 through 15 were tested with a simulated rod weight of 120 lb to duplicate the actual control rod weight. Selected runs were filmed, since it was found that the collapsing characteristics were essentially the same from one run to the next. Shock absorber samples 16 and 17 were tested with one neutron absorber container.

The deceleration rate or impact force was measured on each test, but because of the tangling of the lead wires, the instrumentation failed to operate on three of the tests. The introduction of a load cell below the graphite blocks resulted in a more reliable indication.

The first set of shock absorber bellows was made from 0.016-in.-thick Type 321 stainless steel, and the convolutions had straight sides (a saw tooth profile). The tests were performed in a compression testing machine, and it was found that a constant 600 lb of force would collapse the convolutions. Four of the bellows were assembled for the drop test.

The drop tests indicated that the shock absorbers would perform satisfactorily. The bellows were found to form into an "S" shape as they collapsed but they did not damage the channel or wedge in it. Deceleration rates of about 75 g were recorded when a 148-lb rod was dropped from 12.5 ft with the shock absorber at ambient temperature. There was no damage to the graphite block.

The next bellows was formed of 0.016-in.-thick Incoloy 800, and the convolutions had straight sides. Considerable difficulty was experienced

Fig. 1.22--Lower section of drop test rig

in forming the convolutions because of the deep draw required by the straight sides. After several tooling changes and numerous ruptures of the material during forming, a sample was produced (No. 5). The shock absorber was assembled and tested in the drop-test rig. With a 120-lb simulated rod dropped 15.5 ft and the shock absorber heated to 675°F, the deceleration rate was found to be 150 g. There was no damage to the graphite block or the guide tube and the bellows collapsed uniformly.

Following these tests, the convolution was modified to a more rounded form. The convolutions with a 3/4-in. pitch and a 3/8-in. depth were found to be relatively easy to form in 0.016-in.-thick Incoloy 800. These three samples (Nos. 6, 7, 8) were then drop-tested from 15.5 ft with a simulated rod weight of 120 lb and with the shock absorber heated to approximately 800°F. A typical bellows before impact is shown in Fig. 1.23 and after impact in Fig. 1.24. The deceleration rates were found to be 120 and 116 g, with no evidence of damage to the graphite block or the guide tube. Nine additional shock absorbers formed of 0.016-in.-thick Incoloy 800, with rounded convolutions were then tested. Concurrently the design of the neutron absorber containers for the control was being developed (see Fig. 1.25). Eight absorber containers were produced of Incoloy 800 with a cast spine attachment and graphite filler material.

The drop tests with six shock absorbers (Nos. 9, 10, 12, 13, 14, and 15) were performed with two neutron absorber containers mounted on a section of control rod spine. Additional weight was supplied by the previously used simulated rod to produce the desired 120-lb weight. The shock absorber, which was mounted below the containers, was attached in the same manner as is planned for the final control rod design. The results of the tests are shown in Table 1.5. It was found that the deceleration rates were slightly lower than in the previous tests because the control rod spine was absorbing a portion of the impact and being deformed (see Fig. 1.26). The rod section remained intact, and there was no damage to the neutron absorber containers. A post-test dye-penetrant inspection did not reveal any weld cracks or fractures in the support casting. There was no distortion of the tube section of the containers, and the simulated rod assembly was easily removed from the channel.

Shock absorber Nos. 16 and 17 were tested with a single neutron absorber container attached. The test was to simulate a broken spine accident when the control rod was in the rod-out position. The shock absorber was found to operate properly, and there was no damage to the absorber-container graphite block or to the guide tube. The bellows partially collapsed and formed in an "S" shape, but it did not wedge in the channel (see Fig. 1.27). There was no permanent deformation of the spine.

The final test of the control rod shock absorber was performed with a complete control rod that had been fabricated for the control rod drive and orificing system test (see Fig. 1.28). Shock absorber No. 11 was heated to 800°F and the 120-lb control rod was dropped from 15.5 ft. The deceleration rate was found to be 71 g maximum. There was no damage to the graphite block or the channel. The control rod spine was bent similarly to the previous tests (see Fig. 1.28). The upper section of the rod spine was less severely

Fig. 1.23--Control rod shock absorber, sample No. 6; 0.016 Incoloy 800

Fig. 1.24--Control rod shock absorber, sample No. 6; 0.016 Incoloy 800, heated to 980°F, dropped 15.5 ft, rod weight 120 lb

Fig. 1.25--Neutron absorber assembly; 0.05-in.-thick Incoloy 800 tubing, 0.08-in.-thick Incoloy 800 end caps, neutron absorber simulated with graphite

Table 1.5
TEST CONDITIONS AND RESULTS

Shock Absorber No.	Date Tested	Rod Wt (lb)	Distance Dropped (ft)	Shock Absorber Temp (°F)	Max. Decel. (G)	Max. Force (lb)	Bellows Material (0.016-in. Thick)	Convolution Shape	Rod Configuration
1	1/24/67	148	12.5	70	70	10,400	321 S. S.	Straight	Solid weight
2	1/24/67	148	12.5	70	80	11,800	321 S. S.	Straight	Solid weight
3	1/25/67	148	17.0	70	--	---	321 S. S.	Straight	Solid weight
4	1/25/67	148	10.0	70	--	---	321 S. S.	Straight	Solid weight
5	8/8/67	120	15.5	675	150	18,000	Incoloy 800	Straight	Solid weight
6	11/8/67	120	15.5	900	--	---	Incoloy 800	Rounded	Solid weight
7	12/6/67	120	15.5	800	120	14,400	Incoloy 800	Rounded	Solid weight
8	12/6/67	120	15.5	800	116	14,000	Incoloy 800	Rounded	Solid weight
9	1/23/68	120	15.5	200	96	11,500	Incoloy 800	Rounded	Solid weight + two absorber containers
10	1/23/68	120	15.5	200	94	11,300	Incoloy 800	Rounded	Solid weight + two absorber containers
11	2/23/68	120	15.5	800	71	8,500	Incoloy 800	Rounded	Complete control rod
12	2/6/68	120	15.5	800	106	12,800	Incoloy 800	Rounded	Solid weight + two absorber containers
13	2/6/68	120	15.5	800	106	12,800	Incoloy 800	Rounded	Solid weight + two absorber containers
14	2/8/68	120	20.0	800	113	13,600	Incoloy 800	Rounded	Solid weight + two absorber containers
15	2/12/68	120	20.0	800	113	13,600	Incoloy 800	Rounded	Solid weight + two absorber containers
16	1/24/68	13	15.5	800	58	700	Incoloy 800	Rounded	One absorber container
17	1/24/68	13	15.5	800	58	700	Incoloy 800	Rounded	One absorber container

Fig. 1.28--Control rod and shock absorber assembly used in final drop test. Shock absorber No. 11 was heated to 800°F , dropped from 15.5 ft, total assembly weight 120 lb, deceleration 71 g. The complete assembly is shown at left; disassembled components showing bent spine are at right

bent, because of the lighter loads that were applied. All welds on the spine attachment collars were intact, and there were no fractures in the spine. The neutron absorber containers were not damaged. There was no inspection to determine the damage to the graphite filler, since these containers are to be reused. The control rod was easily removed from the channel, and there was no evidence of wedging of either the control rod or the shock absorber.

A curve showing the impact load is given in Fig. 1.29. The initial portion of the curve, up to 0.025 sec, is the initial collapse of the bellows convolutions. The remaining load rise occurs as the convolution edges are flattened. The control rod rebounded from the initial impact and reached a calculated height of 5 in. The second and last reported impact resulted in a 1250-lb force (10 g).

The test program demonstrated that the collapsible shock absorber absorbs sufficient energy from a free-falling control rod to prevent damage to the lower core reflector blocks. The shock absorber collapses into a uniform shape and would permit easy removal from the core. The integrity of the neutron absorber container and attachment spine was proved during a simulated reactor accident, demonstrating that the control rod would remain in one unit. The absorber container did not rupture or distort and would permit easy removal. The partial collapse of a shock absorber was demonstrated, and again it was found that the unit could be easily removed from the reactor.

References

1. U. S. Department of the Interior, Bureau of Reclamation, Concrete Manual, 7th ed., Denver, Colorado, 1963.

Fig. 1.29--PSC control rod shock absorber drop test; load cell trace load versus time

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.

Nuclear Design Status

The Reference Design 6 analysis was carried through the first five years of reactor lifetime using single-channel synthesis depletion calculations. This design is characterized by two axial fuel zones and the use of silicon carbide (TRISO)-coated fuel particles. Reactivity behavior and fuel temperatures appear to be acceptable.

Flux Synthesis Lifetime Study of Reference Design 6, Axially Zoned Core

The Reference Design 5, which was described in the previous quarterly report (GA-8600), was an axially unzoned core using BISO-coated fuel particles. Unacceptable axial flux tilts resulted in connection with partially inserted control rods leading to excessively high fuel temperatures in the bottom half of the core.

Reference Design 6 is characterized by two axial fuel zones so designed that the average unrodded core power distribution is tilted toward the top of the core, the gas inlet end. Partially inserted rods will, of course, reduce that tilt and tend to shift the power toward the bottom half; however, acceptable fuel temperatures are obtained.

Reference Design 6 also includes loading adjustments to compensate for the introduction of SiC in connection with TRISO-coated fuel particles and to allow for impurities and uncertainties. The total initial core loading is as follows: U, 1098 kg; Th, 19,435 kg; C, 163,800 kg; Si, 4353 kg.

Previous studies have shown that two axial zones give a reasonable axial power and temperature distribution. Each zone comprises three layers of fuel elements, or half the core.

Besides the axial zoning, radial fuel zoning is employed. As reported previously, the initial core is a partial mockup of the equilibrium cycle. This decision is based on considerations of thermal performance and economics. Low peaking factors are obtained with only modest economic penalties.

As a result of these requirements, 16 fuel compositions are utilized in the initial core. Table 2.1 summarizes the fissile and fertile material loadings in these compositions and the number of fuel elements available for each mixture.

Table 2.1
INITIAL CORE

Composition	Total No. of Elements	Th (kg)	U (kg)	Lumped Poison Stick Diam (in.)	Lumped Poison Concentration (g B _{nat} /cm ³)
1	282	3858	102.3	---	---
2	120	1729	88.3	---	---
3	120	1332	55.2	---	---
4	63	793	53.0	0.4	0.066
5	63	679	39.2	0.4	0.066
6	126	1601	125.4	0.4	0.066
7	126	1402	93.1	0.4	0.066
8	171	1870	175.2	0.45	0.060
9	171	1668	132.9	0.45	0.060
10	21	283	14.8	0.4	0.03
11	21	233	10.6	0.4	0.03
12	42	588	36.3	0.4	0.03
13	42	493	25.8	0.4	0.03
14	57	729	53.7	0.4	0.03
15	57	630	39.6	0.4	0.03
16	324 ^a	1546	52.9	(a)	(a)

^aComposition 16 fills the four-fuel-hole buffer region at core-reflector interface. Sections of 324 fueled blocks are involved. Lumped poison sticks are same as in inner section of element.

Figure 2.1 shows the spatial distribution of the various types in the top and bottom halves of the core. It should be noted that several refueling regions of the initial core are not axially zoned. These are the regions that are refueled after two years of reactor operation. They are overloaded with Th, and have a low U content to compensate for the low fission-product inventory in the initial core and thus exhibit a fairly low peaking factor during their two-year residence time in the core. Composition 16 comprises the entire buffer zone at the core reflector interface, four fuel holes thick; here again, no axial zoning is employed.

Lumped poison is loaded into the initial core to shape the reactivity behavior. Only the regions residing more than three years in the core contain it. The poison itself is axially uniform. Two pins per element are used in the affected regions. Poison stick diameters and concentrations are also summarized in Table 2.1 for the initial core. Five fuel compositions

are used in each of the first three reload segments. They are summarized in Table 2.2. As in the initial core, fuel in the buffer region at the core-reflector interface is not axially zoned. The total fissile material feed varies slightly for each reload to provide sufficient excess reactivity at the end of the cycle, a consequence of the not-ideal initial core mockup. Each fuel element in the reload segment contains six lumped burnable poison sticks 0.5 in. in diameter. Again, the lumped poison is not axially zoned. However, two poison stick concentrations are used to improve the transverse power distribution. Fuel elements loaded into the third ring of regions (i.e., refueling regions 8 through 19), obtain a 20% heavier poison load than the remaining regions.

As in the analysis of Reference Design 5, two transverse cuts were used, one for each axial fuel zone. The cuts differ in their loading composition, average fuel and moderator temperature applicable to the cross sections, and the presence or absence of control rods.

A major step in the Reference Design 6 analysis is the automation of the depletion steps and the subsequent thermal analysis of the core by means of the SCANAL code. Transverse flux distributions for both slices are obtained with a two-dimensional depletion code, together with average macroscopic properties in each slice, which are used to compute an average axial flux distribution in a one-dimensional diffusion code. These results are synthesized to establish a three-dimensional power distribution of the core, the smallest unit being one fuel element. Coolant flow through each refueling region is then computed, assuming ideal orificing for constant gas outlet temperature. Peaking factors in each column of a refueling region are utilized to distribute the flow into each column, and centerline and average rod temperatures are computed for each column. Finally, a summary is given showing average fuel rod and rod centerline volume fractions running above any given temperature. Thus, the effectiveness of fuel zoning and control rod programs is evaluated directly by studying the fuel temperature, the most important parameter of the core.

Five years of reactor history have been analyzed and significant results are summarized in Table 2.3. Core reactivity deviates by about $\pm 0.5\% \Delta k$ from unity, a result of restricting control rod positions to being in a completely inserted, 50% withdrawn, or completely withdrawn position in this analysis. Sufficient reactivity is provided at any time in life. Excess reactivity toward the end of cycle may be somewhat high, by about $1.5\% \Delta k$, confirming the loading adjustments reported in the previous quarterly report (GA-8600). Further refinements will be made if more information becomes available to permit a reduction in the uncertainties.

The stability of the axial power distribution has to be evaluated with respect to the uniform axial fuel loading depletion studies for Reference Design 5. With three partially inserted rods, the power stays mostly tilted toward the top and acceptable fuel temperatures result. Although the axial power distribution tends to become flatter during the end of each cycle and slightly tilts toward the bottom at the end of the second cycle, no excessive fuel temperatures are observed. The highest fuel temperatures and peaking factors are calculated early in each refueling cycle when many control rods

Table 2.2
REFERENCE DESIGN 6 RELOAD SEGMENTS

Reload Number	Composition	Number of Elements	Th (kg)	U	Regions Loaded with this Composition	Lumped Poison Diam (in.)	Lumped Poison Loadings (g B _{nat} /cm ³)
1	1	63	655	61.5	5,10,17, upper	0.5	0.025 (0.031)
	2	63	607	46.0	5,10,17, lower	0.5	0.025 (0.031)
	3	57	452	55.5	21,28,35, upper	0.5	0.025
	4	57	430	42.0	21,28,35, lower	0.5	0.025
	5	54 ^a	214	3.8	Buffer	0.5	0.025
	Total	240	2358	214.8			
2	1	63	655	67.8	4,8,15, upper	0.5	0.025 (0.031)
	2	63	607	50.7	4,8,15, lower	0.5	0.025 (0.031)
	3	57	452	61.2	25,32,36, upper	0.5	0.025
	4	57	430	46.3	25,32,36, lower	0.5	0.025
	5	54 ^a	214	10.8	Buffer	0.5	0.025
	Total	240	2358	236.8			
3	1	63	655	68.2	3,13,18, upper	0.5	0.025 (0.031)
	2	63	607	50.8	3,13,18, lower	0.5	0.025 (0.031)
	3	57	452	61.5	22,29,33, upper	0.5	0.025
	4	57	430	46.5	22,29,33, lower	0.5	0.025
	5	54 ^a	214	10.9	Buffer	0.5	0.025
	Total	240	2358	237.9			

^a Only the buffer region of these 54 elements is involved.

Table 2.3
SUMMARY OF REFERENCE DESIGN 6 SYSTHESIS STUDY

Time in Life EFPD ^a	Number of Rods Inserted		K _{eff}			Power Fraction (%)		Percent of Fuel Above 2000°F	
	Top Half	Bottom Half	Top Cut	Bottom Cut	Net Core	Top Half	Bottom Half	Average Rod	Rod Centerline
Initial									
0	13	10	1.005	0.996	1.002	67	33	0.4	7.4
2	7	4	0.991	1.000	0.994	61	39	0.07	5.8
5	4	4	1.004	0.996	1.001	67	33	0.0	1.4
10	1	4	0.999	0.994	0.997	70	30	0.1	5.0
50	1	4	1.000	0.988	0.997	68	32	0.0	2.9
104	1	4	0.998	0.990	0.996	67	34	0.02	2.2
207	1	4	1.002	1.001	1.001	64	36	0.0	0.8
311	4	4	0.994	1.008	0.998	58	42	0.0	0.02
414	4	4	0.994	1.013	0.999	55	45	0.0	0.1
518	1	4	1.018	1.013	1.004	60	40	0.01	0.9
621	1	4	0.990	1.011	0.996	55	45	0.0	0.03
Reload 1									
0	13	10	1.00	1.004	1.003	53	47	0.1	3.8
2	7	4	0.994	1.015	1.006	46	54	0.1	3.2
5	7	4	0.993	1.013	1.004	47	53	0.02	2.6
10	7	4	0.993	1.000	1.003	48	52	0.0	1.9
50	7	4	0.989	1.002	0.996	50	50	0.0	0.7
104	4	4	0.999	1.004	1.001	53	47	0.0	0.4
207	4	1	0.997	1.011	1.004	49	51	0.0	0.6
311	4	1	0.990	1.006	0.998	48	52	0.0	0.5
Reload 2									
0	13	10	1.002	0.991	0.998	60	40	0.4	4.2
2	7	4	1.001	1.002	1.002	55	45	0.2	3.2
5	7	4	0.997	1.002	1.001	54	46	0.0	1.2
10	7	4	0.996	1.001	0.999	54	46	0.2	1.6
50	4	4	1.007	1.000	1.005	58	42	0.0	0.1
104	4	4	1.005	0.998	1.003	58	42	0.0	1.1
207	4	1	1.00	1.007	1.004	52	48	0.0	0.6
311	4	1	0.995	1.001	0.998	52	48	0.0	0.6
Reload 3									
0	13	10	1.007	0.991	1.002	61	39	0.5	5.2
2	7	4	1.003	1.000	1.003	57	43	0.8	4.1
5	7	4	0.999	1.000	1.000	55	45	0.0	1.2
10	7	4	0.999	0.998	0.999	55	45	0.2	2.0
50	4	4	1.009	0.998	1.005	59	41	0.0	1.4
104	4	4	1.006	0.999	1.004	58	42	0.0	1.2
207	4	4	1.003	0.996	1.000	58	42	0.0	1.6
311	4	1	0.995	1.003	0.999	51	49	0.0	0.8

^a Equivalent full-power days.

are inserted, before Xe^{135} and Pa^{233} have equilibrated. For a short period several percent of the fuel rod centerlines exceed 2000°F , but during most of the cycles, less than 1% of the centerlines is above this temperature.

Peaking factors at some time points during the initial and the first three reload cycles are shown in Table 2.4. Neglecting the perturbations early in each cycle, peaking factors stay well below the limit of 1.83. It appears that both rod program and axial zoning for Reference Design 6 are adequate.

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.

Alternative Control Rod Materials

The present control rod containing boronated graphite absorber material has an estimated life of about six years assuming a 100% duty factor. Withdrawn rods have a predicted life of approximately 12 years. Radiation damage requires periodic replacement of these rods.

Alternative absorber materials were examined as a means of extending the lifetime of the control rods. The materials investigated are hafnium, erbium, and pyrohafnates (a mixture mainly of hafnium, erbium, and dysprosium with minor concentrations of holmium, terbium, thulium, and ytterbium). The use of these materials would probably result in rods that initially are considerably more expensive than the current design. However, the longer lifetime expected may lead to a net reduction of cost.

In the study performed, rod outer diameter and reactivity worth were the prime parameters. The following material requirements were determined:

Reference Design, B_{nat}	1.0 g/cm ²
Hafnium	14.0 g/cm ²
Erbium	>50 g/cm ²
Pyrohafnates	13.0 g/cm ²

Erbium cannot be used to obtain the required control rod strength in connection with the given rod dimensions. Hafnium and pyrohafnates can provide a sufficient amount of reactivity, if a considerable weight increase can be accepted. These alternative materials will lead to control rods about four to five times heavier than the reference design.

Equilibrium Cycle Rod Worths and Temperature Coefficients

The equilibrium cycle accident analysis to date is based on a rod worth of 1.5% Δk and end-of-cycle temperature coefficients. This data set was

Table 2.4
REGION PEAKING FACTORS VERSUS TIME^a

BOC	Initial Core					First Reload		Second Reload		Third Reload	
	Two Days	Five Days	Ten Days	One Year	EOC	BOC	EOC	BOC	EOC	BOC	EOC
[0.57]	[0.57]	[0.46]	[0.40]	[0.52]	[0.55]	[0.67]	[0.56]	[0.75]	[0.58]	[0.77]	[0.59]
1.27	1.26	1.02	0.93	1.18	1.10	1.25	0.89	0.89	0.80	1.02	0.74
1.10	1.11	0.93	0.86	1.07	1.00	1.07	0.86	1.08	0.80	1.81 R	1.44
1.36	1.34	1.12	1.08	1.08	1.00	1.14	0.87	1.81 R	1.39	1.85	1.36
0.87	0.84	0.74	0.67	0.76	0.78	1.53 R	1.33	1.88	1.30	1.46	1.05
1.29	1.24	1.04	0.96	1.05	1.01	1.12	1.01	1.17	0.94	0.99	0.78
1.24	1.22	1.00	0.89	1.13	1.07	1.11	0.94	0.97	0.82	0.86	0.73
1.14	1.15	0.99	1.01	0.99	0.96	1.10	0.97	0.95 R	1.35	1.55	1.09
[0.79]	[0.82]	[0.94]	0.07	1.31	1.20	1.40	1.10	[0.69]	0.96	1.32	0.89
0.60	0.64	0.66	0.61	0.80	0.89	1.08 R	1.29	1.40	1.07	1.32	1.01
1.49	1.52	1.26	1.27	1.26	1.27	[0.77]	1.02	1.34	0.91	[0.78]	0.99
1.31	1.34	1.25	1.34	1.28	1.28	1.27	0.94	1.11	0.86	0.94	0.93
[0.84]	[0.83]	[1.01]	1.18	1.24	1.13	1.41	1.00	[0.92]	0.99	1.48 R	1.44
1.38	1.32	1.38	1.25	1.25	1.15	1.31	1.07	1.32	1.01	0.83	0.91
1.59	1.49	1.26	1.20	1.00	1.00	[0.74]	0.98	1.60 R	1.42	[0.86]	1.19
1.41	1.35	1.26	1.27	1.13	1.11	1.04	1.04	0.91	0.97	0.91	0.98
[0.49]	[0.48]	[0.58]	0.64	0.81	0.84	1.18 R	1.33	[0.80]	1.17	1.21	1.04
1.10	1.18	1.14	0.99	1.14	1.02	0.84	0.99	0.84	1.02	1.07 R	1.37
1.63	1.60	1.28	1.18	1.25	1.18	[0.78]	1.00	0.92	0.98	[0.64]	0.85
0.93	0.97	0.89	0.97	0.83	0.92	[0.71]	1.09	0.70	0.92	[0.66]	0.70
[0.39]	[0.43]	0.58	0.56	0.65	0.67	1.50 R	1.51	[0.96]	1.17	1.37	0.84
0.68	0.76	1.01	0.93	1.15	1.09	[0.74]	[0.95]	0.90	0.90	[1.13]R	[1.06]
[0.54]	[0.59]	1.07	0.95	1.12	1.16	0.82	0.95	[0.63]	0.76	0.83	0.71
1.05	1.11	1.09	1.14	1.08	1.23	[0.79]	1.02	1.07	0.81	[0.73]	0.84
[0.68]	[0.72]	[0.62]	[1.03]	[0.58]	[0.88]	0.74	0.77	[0.95] R	[0.92]	1.08	1.32
0.98	1.04	1.05	1.21	1.05	1.17	[0.70]	0.84	0.87	0.68	[0.45]	0.76
[0.80]	[0.85]	1.20	1.21	1.28	1.25	1.29 R	0.96	[0.94]	0.83	0.66	0.75
0.48	0.49	0.70	0.64	0.73	0.74	[1.14]	[1.15]	1.58	1.19	[0.60]	[0.85]
[0.69]	[0.68]	1.26	1.08	1.09	1.00	1.11	0.90	[1.71]	0.82	0.79 R	1.16
1.45	1.39	1.39	1.33	1.12	1.14	[1.11]	0.98	0.96	0.84	[0.51]	0.79
[1.08]	[1.04]	[0.91]	[1.28]	[0.70]	[0.96]	1.04	1.01	[0.61]	[0.82]	0.67	0.91
1.00	0.98	0.98	1.06	0.74	0.76	[0.45]	0.71	0.82 R	1.16	[0.68]	1.37
[0.90]	[0.91]	1.25	1.17	1.15	1.03	0.78	0.92	[0.58]	0.90	1.21 R	1.62
0.90	0.92	1.19	1.03	1.17	1.06	[0.57]	[0.91]	0.74	1.00	[0.59]	[0.84]
[0.39]	[0.40]	0.69	0.57	0.63	0.60	0.83 R	1.22	[0.62]	1.30	0.89	0.93
1.10	1.09	1.00	0.93	0.80	0.77	[0.54]	0.81	0.99 R	1.62	[1.05]	1.25
[0.94]	[0.95]	[0.77]	[1.07]	[0.68]	[0.91]	0.94	1.04	[0.52]	[0.92]	0.91	0.84

^a [] = rodged regions; R = refueled regions; BOC = beginning of cycle; EOC = end of cycle.

examined and appears to be highly conservative. At most, four rod pairs are expected to be inserted towards the end of a cycle, controlling about 1.5% to 2% of reactivity, making a 1.5% rod withdrawal accident excessively conservative. Only at the beginning of a cycle, when more rod pairs are inserted and before Xe^{135} and Pa^{233} are equilibrated, are rod worths of the order of 1.5% Δk possible.

Temperature coefficients at the beginning of a cycle are typically $0.4 \times 10^{-5}/^{\circ}\text{C}$ more negative than at the end of a cycle, because of the higher U^{235} loading, the lower U^{233} loading, the harder spectrum (and thus higher doppler coefficient), and the presence of control rods. It may therefore be concluded that a 1.5% rod withdrawal accident at the beginning of a cycle will lead to accident conditions less severe than using a 1.2% Δk rod withdrawal accident at the end of a cycle.

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.

Effects of Core Impurities on Reactivity and Fuel Cycle Cost

Up to 50 different elements will be present as impurities in the core. Most of these impurities are absorbers, decrease the core reactivity, and increase the fuel cycle cost. Impurity limits for the various core components are required to keep uncertainties in core reactivity as low as possible and to minimize cost penalties. A summary of reactivity and fuel cycle cost worth is given below for all impurities of interest.

The presence of 1 g of natural boron in the core decreases the core reactivity by $\Delta\rho_B = 2.3 \times 10^{-5}$ and increases the fuel cycle cost by 1.2×10^{-4} mill/kw-hr. This assumes that the boron is a permanent poison and does not burn out. Most of the impurities are nonburnable.

The reactivity worth of other absorbers is conveniently expressed in terms of boron equivalents. Knowing the reactivity worth of 1 g of boron, $\Delta\rho_B$, 1 g of any other absorber X reduces the core reactivity by

$$\Delta\rho_X = \Delta\rho_B \frac{\sigma_X}{A_X} \frac{A_B}{\sigma_B},$$

where A_B = atomic weight of boron,

σ_B = effective cross section of boron,

A_X = atomic weight of material X, and

σ_X = effective cross section of material X.

The quantity $(\sigma_X/A_X) (A_B/\sigma_B)$ is called the boron equivalent of material X. Boron equivalents for several materials are listed in Table 2.5.

Table 2.5
BORON EQUIVALENTS

Material	Boron Equivalent	Material	Boron Equivalent
Li	0.146	Zr	0.00010
B	1.0	Mo	0.0015
N	0.002	Ag ¹⁰⁹	0.053
O	0.000004	Cd	0.862
Na	0.00034	Sn	0.00028
Mg	0.000053	La	0.001
Al	0.00013	Ce	0.0001
Si	0.000083	Pr	0.002
P	0.00010	Nd	0.006
S	0.000285	Sm	0.914
K	0.0010	Eu	0.474
Ca	0.0002	Gd	1.487
Ti	0.0018	Tb	0.02
V	0.0015	Dy	0.11
Cr	0.00085	Ho	0.17 ^a
Mn	0.0041	Er	0.08
Fe	0.00067	Tm	0.35 ^a
Co	0.012	Yb	0.08 ^a
Ni	0.0011	Lu	0.05
Cu	0.0010	Pb	0.000012
Zn	0.00027	Bi	0.000032

^a-Estimates.

Hydrogen affects the PSC core in two ways: (1) because the core is undermoderated, the reactivity is increased by the addition of moderators; and (2) the control rod worth, and thus the shutdown margin, are reduced because a smaller migration area results from the addition of moderators. The reactivity worth of H₂O is $4.6 \times 10^{-8} \Delta k/g \text{ H}_2\text{O}$ in core excess reactivity and $6.2 \times 10^{-8} \Delta k/g \text{ H}_2\text{O}$ in core shutdown margin. The reactivity worth of hydrogen is $4.2 \times 10^{-7} \Delta k/g \text{ H}$ in core excess reactivity and $5.6 \times 10^{-7} \Delta k/g \text{ H}$ in shutdown margin.

Fuel Element Exposure

Design limits on the fuel element exposure are 20% FIMA (fissions per initial metal atom) for the fissile particles and 7% FIMA on fertile particles, with a maximum fast-flux dose of $8 \times 10^{21} \text{ nvt}$. Significant portions of the initial core fuel will never reach these limits, as shown in Figs. 2.2 and

Fig. 2.2--Fissile particles initial core

2.3. The graphs show volume fractions of fuel discharged after 2, 3, 4, 5, and 6 years that exceed any given exposure. Fuel unloaded after two years, for instance, barely exceeds 10% FIMA on fissile and 1% FIMA on fertile particles with a fast-flux exposure slightly above 2×10^{21} nvt. Fast fluence of fuel discharged after three years is limited to 4×10^{21} nvt. None of the fuel that comes out of the reactor during the first six years of operation exceeds the design limit. These data confirm the exposure rates published in an earlier quarterly report (GA-8420) for six-year-old fuel in the initial core. The figures also show that the fissile particles accumulate the highest fraction of their burnup early in life, about 50% of the total during the first two years. During this period, freshly loaded U^{235} is mostly burned. Fertile particles have the fastest burnup rates late in life when significant amounts of U^{233} have formed.

Advantages may result from using different particles in sections of the core obtaining only low or modest exposures, a point to be further investigated.

Fuel Hole Diameters

The initial core is composed of 16 fuel mixtures dictated by radial and axial zoning requirements. To ensure that all of the holes are filled and that all fuel fits exactly into the space provided, a maximum of 16 fuel hole sizes is required. Because this approach was quite complicated and difficult for design and fabrication purposes, it was simplified by adopting three fuel hole sizes: 30/64 in., 32/64 in., and 35/64 in. A full hole for a given metal loading would then require the use of thicker coatings on some particles, or more matrix material.

Further investigations led to a standardization of fuel hole diameters at 0.5 in. in all sections of the core. This is a significant step in simplifying the core design. It will make drill procurement much easier and will help to reduce uncertainties in the nuclear design.

The nominal standard fuel hole diameter now is smaller than the maximum fuel hole diameter established previously, which will increase the stability and strength of the blocks under irradiation. To ensure that all the holes will obtain the required fuel loading, the reference particle dimensions given in Table 2.6 have been adopted. The total size range of kernels will

Table 2.6
PARTICLE DIMENSIONS

Particle	Kernel Diam (μ)	Total Coating Thickness
Fissile: A	100-175	120 $\pm$ 15
B	175-275	130 $\pm$ 15
Fertile: A	300-450	130 $\pm$ 15
B	450-600	140 $\pm$ 15

Fig. 2.3--Fertile particles initial core

be screened to obtain two sizes for both fissile and fertile particles, varying the effective metal load per unit particle volume. Blending of the material from the different batches will be performed such that proper fuel loadings and particle volumes are achieved.

Task III
FUEL DEVELOPMENT

FUEL ELEMENT DESIGN

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

Mechanical Design

The specification for fuel element graphite components, including manufacturing drawings for the graphite fuel element components, were issued for inquiry during the quarter.

The decision was made this quarter to adopt a single fuel hole size of 0.500 in. diameter, replacing the three sizes presently shown on the drawings. The single hole size will simplify manufacturing the elements and reduce the number of spare machined blocks required.

Thermal and Coolant Flow Design

The side reflector flow distribution calculations have been reviewed, and the results are listed in Table 3.1. These values are based on an estimated pressure drop of 7.0 psi across the core and include the effects of flow-restricting strips between the side reflector support blocks. Before the incorporation of these strips, flow between the reflector support blocks had been estimated to be approximately 3.8% of the total flow.

Table 3.1
SIDE REFLECTOR FLOW DISTRIBUTION

Location	Flow (lb/hr)	Total Circ. Flow (%)	Exit Temp (°F)
Reflector-barrel	73,000	2.1	900
Between reflector blocks	28,000	0.8	950
Core-reflector gap	127,000	3.6	1250

It should be noted that the values reported here are predictions of the coolant flow that enters the lower plenum via openings between the core support blocks (and barrel) beneath the side reflector zone. Since the core channels, because of orificing, are generally at a lower pressure than the side reflector in a given horizontal plane, the magnitude of the bypass flows would be somewhat larger at the top of the core, diminishing by crossflow effects to the values reported at the level of the core support floor.

The outlet temperatures of 900° and 950°F for flows (1) and (2), respectively, are reported previously. However, core crossflow analyses to date have shown that flow (3), the core-reflector gap flow, removes a significant portion of heat from the adjacent fuel elements and exits at a higher temperature than previously predicted. This gap flow is expected to reach an average exit temperature of 1250°F for a heat removal of about 2.5% of gross core thermal power.

The analysis of flow distribution in the core continued in order to improve the accuracy of the analytical results obtained thus far. During the reporting period work has been directed toward development of a 12-region analytical flow model to supersede the seven-region model previously used and development of an auxiliary program that utilizes the results from the flow distribution analysis to calculate region outlet temperatures.

The 12-region model is expected to produce a more accurate determination of the over-all core pressure drop plus a detailed core flow distribution from which fuel temperatures and coolant temperatures can be analyzed. This is accomplished by setting up three distinct portions of the core, each having 12 regions. The central fuel region is analyzed separately by assuming that the irradiation-induced dimensional changes produced in the graphite fuel elements of this region are essentially uniform and that crossflow effects will therefore be negligible.

The 12-region analytical model is similar to the seven-region model, but has two main differences. In the seven-region model, crossflow between the primary coolant holes and the gap between fuel element columns was permitted only in the columns immediately adjacent to the side reflector. In the 12-region model, the crossflow characteristics of the columns next to side reflector are the same as in the seven-region model, but unlike the seven-region model, crossflow is permitted in all interior fuel regions. The irradiation-induced dimensional changes in these interior regions are not expected to be large; however, a 0.010-in. gap is assumed for all interfaces in the interior regions. This compares with an equivalent interface gap of 0.002 in. determined experimentally for new elements. A second difference in the two models is the amount of flow area allocated to the flow path between fuel regions. In the 12-region model, an average of 13% of the coolant flow area is allocated to the gap between the fuel element columns versus 9% in the earlier model. This increase is due primarily to the fact that half of the gap at the core-reflector boundary was included in the seven-region model, whereas the full gap is included in the present model.

Problems were encountered in obtaining a converged solution with the 12-region model during the initial runs, but these were ultimately attributed

to minor errors in input data and not to any inherent feature of the computer program. It was found that the amount of input preparation required to describe the flow network is quite large, and therefore susceptible to subtle errors. In order to verify the correctness of the input data, it was necessary to make numerous cross checks of the input.

An auxiliary program has been prepared to utilize the results of the flow distribution program to calculate region outlet temperatures. The code has been debugged and is considered operational. The essential features of the program are: (1) each region is assigned a radial and axial power distribution, (2) the power absorbed by the coolant flow in the internal bypass gaps (gaps between columns that belong to the same fuel region) is so calculated that the coolant temperature is the same as that in the primary coolant holes of that region, and (3) the power absorbed by the coolant flow in the external bypass gaps (gaps between columns belonging to different regions) is calculated based on a specified coolant temperature as a function of axial position. The specified coolant temperature is based on results from a three-dimensional thermal analysis of a portion of a fuel element column that included several individual coolant holes and the bypass gap between columns. The coolant outlet temperature in the external gaps was found to be dependent on the relative power of the neighboring fuel regions as shown in Fig. 3.1. It may be noted that the coolant outlet temperature of the external gap is appreciably lower (100° to 200°F) than the design mixed-mean outlet temperature of the core (1470°F). Heat balance in the core is accomplished by assuming that the heat gained by the coolant in the external gaps acts to decrease the heat input to the primary coolant holes in proportion to the radial power factor of the affected region. The temperatures at each node in the flow network are then determined by a set of simultaneous equations formed by making a heat balance about each node.

Several results from the flow distribution analysis have been obtained for reactor operating conditions corresponding to 6, 7, and 8 psi. For these calculations, the region orifice settings were first estimated from the results previously obtained on the seven-region model and corrected once in order to obtain a more uniform core outlet temperature. An evaluation of the results obtained thus far indicates that the region flow control orifice settings that were used in the analysis must be modified appreciably in order to achieve a uniform exit temperature. The orifice settings have a significant effect on over-all core ΔP , and several iterations will be required to achieve a representative combination of orifice settings.

Analyses of the flow and pressure data from the full-scale orifice tests have been completed. A value of 5.15 for K_{min} , the wide-open-orifice pressure loss coefficient, has been established for reactor conditions based upon the test data. (K_{min} is defined as the irrecoverable pressure loss from the plenum above the orifice into a coolant hole expressed in local coolant hole velocity heads. It includes an irrecoverable coolant hole entry loss of 0.35 velocity heads.) Figure 3.2 shows a plot of the full-scale orifice loss coefficient data versus throat Reynolds number together with similar data taken during calibration of the 0.45-scale multi-column test rig orifices. It is encouraging to note the confirmatory trend of these two independent sets of data, particularly in view of the fact that the multicolumn orifices

Fig. 3.1--Bypass gap exit temperatures versus region radial peaking factor

Fig. 3.2--Core-orifice-wide-open pressure loss coefficient test data

possessed minor scaling imperfections affecting port and throat flow resistances. For an estimated 0.35- to 0.40-psi velocity head in the top reflector elements of a refueling region operating with a 1.80 radial power factor, the irrecoverable ΔP across the orifice will be approximately 1.8 to 2.0 psi, or about 25% of current estimates of total core ΔP .

Before this experimental information was available, core flow distribution analyses were performed using an analytically established value for $K_{\min}$ of 3.7. The current analyses in progress using the three 12-region flow code models incorporate the experimentally determined value of $K_{\min} = 5.15$. It is expected that the most significant effect will be a reduction in flow rate of approximately 5% into regions operating with a wide-open orifice. Previous analyses have shown that a flow reduction of 5% will increase the maximum fuel and coolant exit temperatures by about 40°F.

Figures 3.3 and 3.4 are photographs of the test rig showing the overall configuration and details of the mocked up flow distribution plenum beneath the orifice. Figure 3.5 is a functional schematic of the test rig. Because of highly turbulent conditions in the distribution plenum immediately beneath the orifice, it was found that the most consistent orifice ΔP data were obtained by measuring pressures in the simulated coolant channels, providing the data were taken sufficiently downstream to avoid entrance effects. Further, it was also observed that identical static pressure readings were obtained at the coolant channel exit stations and at the wall of the exit plenum vessel.

In the interest of minimizing test rig complexity, the rig was designed with a fewer number of larger-diameter coolant channels than a fully scaled reactor refueling region. The design provided for properly scaled ΔP versus flow-rate characteristics, but the coolant channel entrance conditions of static and dynamic pressures had to be corrected to conditions commensurate with fully scaled 5/8-in.-diameter coolant hole geometry. This was done by back-calculating from measured pressures to a total pressure immediately above the holes in the mixing plenum beneath the orifice. Treating this total pressure as a fixed quantity, independent of hole size for a given core ΔP and flow rate, coolant channel entrance pressure and velocity head were calculated for an array of 5/8-in. holes, holding the same conditions of flow rate and total rig ΔP . Compressible flow relations and smooth-tube friction correlations were used in making this pressure adjustment.

One of the major sources of error in the resultant value of $K_{\min}$ is uncertainty in the value of channel entrance losses to be used in calculating the adjusted pressures. Parametric studies of this source of error led to an estimate of its effect on $K_{\min}$ of $\pm 6\%$. Venturi mass flow rate measurements were found to be another source of potentially significant errors. It was determined in performing the calculation for the pressure adjustments mentioned above, that perturbations in mass flow rate, as a result of the leverage of the calculational method, caused proportionately larger variations in the value of $K_{\min}$. To minimize this problem, pitot-static pressure traverses were performed in the facility air flow ducting to establish a precise value for the venturi flow coefficient over the range of flows encountered in testing. The results appear to suggest approximately $\pm 1/2\%$ data. For the purpose of

Fig. 3.3--Full-scale orifice vibration and flow test rig

Fig. 3.4--View of flow distribution plenum with orifice removed

Fig. 3.5--Full-scale orifice test rig

error analysis, however, an accuracy of $\pm 1\%$ was assumed, which was found to cause $\pm 6.8\%$ variations in $K_{\min}$. An estimate of major uncertainties in the predicted value of 5.15 for $K_{\min}$ is given in Table 3.2.

Table 3.2
ESTIMATE OF MAJOR UNCERTAINTIES

<u>Source of Uncertainty</u>	<u>$\Delta K_{\min}$</u>
Uncertainty in coolant hole inlet loss coefficient	± 0.32 or $\sim 6\%$
Errors in mass flow rate	± 0.36 or $\sim 7\%$
Errors in ΔP data	± 0.10 or $\sim 2\%$
Curve fit and extrapolation uncertainty	± 0.10 or $\sim 2\%$
Root-sum square	± 0.50 or $\sim 10\%$

As reported previously, data from the 25 pitot-static probes located throughout the test rig coolant channels indicate that the design of the flow distribution plenum beneath the orifice provides for adequate flow distribution to all columns within the region. No significant pressure gradients were found within the plenum to cause maldistributions. Values of coolant hole mass flow rates were found consistent throughout the region within approximately 5% to 7% of the average value. At very low total rig flow rates with the orifice near the closed position there was insufficient velocity head within the coolant channels. To obtain higher velocity head readings from the pitot-static probes. During planned testing of the modified orifice valve, the need for more detailed measurement at very low flows will be evaluated.

Fission-product Transport

Steam Inleakage Analysis. Calculations were continued during this period to estimate the effects of steam inleakage into the primary coolant, subsequent permeation of fuel element graphite, and possible hydrolysis that could occur to core fuel particles that may have experienced coating failure. In the previous quarterly report (GA-8600) several cases of system maloperation leading to steam inleakage were investigated: (1) wrong-loop dump, (2) moisture monitor failure leading to a two-loop dump, and (3) failure to dump. The results showed that less than 0.1% of core fuel particle kernels would be hydrolyzed under the most severe conditions analyzed.

The work carried out in this reporting period included the investigation of a hypothetical case in which a wrong-loop dump has occurred but a two-loop dump does not follow. Core cooling is continued using the leaking loop until steam generator reheater tubes in the dumped loop are cooled to about 450°F. At this point the intact loop is reflooded, the leaking loop is dumped, and core cooldown is continued in a normal manner. Total H_2O inleakage was estimated to be about 15,000 lb with about 1,100 lb of core graphite reacted.

The calculational model considered mass transport of H₂O across the coolant-graphite boundary, transport through the graphite, and reaction with the fuel particle kernel. No allowance was made for the depletion of the H₂O vapor owing to reaction with the element graphite, fuel rod matrix, or particle coating. The analysis showed that 0.12% of the core fuel particle kernels would be hydrolyzed as a result of this sequence of occurrences.

Calculations of subsequent core fission-product release as a result of hydrolyzed fuel particles were made for the metallic species of fission product, in particular Sr⁹⁰. For these calculations it was assumed that a steam inleakage accident occurred at three years and that the fuel was not replaced for the remaining three years of the fuel cycle. Release from the fraction of particles hydrolyzed was assumed to be 100% release for all species.

The fission-product release calculations were made for 879-Mw(t) reactor operating power and assuming the design criteria given in the Preliminary Analysis Safety Report. The results showed that release of the metallic species may be increased by a factor of less than 3×10^{-3} by the end of the six-year fuel cycle following this steam inleakage incident.

Primary Coolant Plateout Monitor System. Design was initiated this period for a system to monitor the concentration of vapor- and aerosol-borne metallic fission products within the reactor primary coolant. The monitoring location was to be downstream from the reactor core exit reflector and upstream from the steam generator tube surfaces in order to provide a representative sample of flow entering each of the two loops within the primary circuit. This monitoring system for the surveillance of direct evaporation release of Sr⁹⁰ and other metallic fission products would be carried out in conjunction with the planned program for sampling noble gas release to the reactor primary circuit.

A small sample probe will be installed at the inlet of one steam generator module upstream from the reheater section tubes within each of the two system loops. Each probe is expected to consist of two main parts: (1) a venturi gas ejector and (2) the sampler containing fractional-micron size metallic filters, an activated charcoal sorption bed, and a small sample flow metering orifice. The components of the sample unit would be arranged in series with the flow control orifice downstream from the filter and charcoal bed. Primary coolant gas flow through the sample unit is produced by connecting the sample outlet to the ejector venturi throat. The gas ejector would be driven by the pressure differential across the steam generator module shroud between the core outlet gas pressure and the helium circulator discharge pressure.

Subsequent design work has established the plateout probe locations in the two loops, and a layout criteria drawing has been prepared to show the locations of the probes and the penetrations for probe access through the PCRV. Design concepts for the probe mounting flange at the steam generator shroud and for probe removal handling tools and mechanisms have been developed.

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

Particle Fuel Systems

Fuel Rod Process Development. Effort has continued on the development of the injection-molding fuel rod fabrication process. The binder formulations

based on R-72S phenolic resin with excess amounts of mixed anhydride curing agent added, which gives a porous fuel rod matrix material, continued to look promising.

After an investigation of several filler material mixes (e.g., GP-38 plus charcoal and GP-38 plus microfine natural flake graphite), it was decided that these offered no great advantage over straight GP-38 sizes to less than 30 μ . Little difference was noted in the physical properties of the finished rods. Also, studies of the cesium sorption of carbon matrix materials have shown that the cesium sorption characteristics of matrix formulations containing added charcoal are virtually identical to those of matrix formulations with no added charcoal. The formulations of current interest therefore, include only GP-38 graphite powder filler; they are presented in Table 3.3.

Table 3.3
MATRIX FORMULATIONS OF CURRENT INTEREST

Formulation	R-72S Phenolic Resin (wt-%)	Anhydride (wt-%)	Nadic Methyl Anhydride (wt-%)	GP-38 Graphite Powder Filler (wt-%)
D-25	49.2	16.4	16.4	18.0
D-38	47.4	15.8	15.8	21.0
D-24	45.6	15.2	15.2	24.0

A number of practice fuel rods using these three formulations have been fabricated in nominal lengths of 1, 6, and 11.5 in. These rods, which were 0.466 in. in diameter after curing, were successfully carbonized unrestrained in a graphite holder with holes of from 0.471 to 0.475 in. in diameter. The rod diameters after carbonizing when measured in six places averaged 0.467 ± 0.001 in. with no value exceeding 0.467 ± 0.002 in.

Fuel Rod Testing. High-temperature exposure tests were conducted on 1-in. fuel rods fabricated with practice particles using matrix formulations D-25, D-38, and D-24. The three rods were heated to 2000°C in 1 hr under vacuum and then to 2700°C in 1/2 hr under 8 psi He pressure. Peak outgassing points occurred at 1470° and 1800°C.

After cooling, the rods were removed and found to be in good condition, with essentially no change in the matrix or rods except that a few additional cracks appeared in the matrix. A few droplets of solidified UC_2 from particle cores that had melted and diffused through the PyC coating at the extremely high temperatures appeared to be extruded from cracks in the matrix. However, the whole rods themselves were completely intact. Measurements on these rods showed a diametral increase of approximately 2 mils and a matrix weight loss of 1% to 2%.

Compression tests were conducted on 1-in. fuel rods fabricated with three different matrix formulations and practice particles. The results

showed compressive strengths (average of three specimens) of 470, 510, and 530 psi for D-25, D-38, and D-33 (25 wt-% filler), respectively.

Hydrogen in Fuel Rods

The effects of gaseous impurities in the fuel rods on their performance under reactor conditions is being investigated. An initial analysis of carbonized matrix material containing no fuel particles revealed significant quantities of hydrogen plus some nitrogen and oxygen. The hydrogen is by far the most important of these impurities. Its presence in the reactor core in significant quantities results in excess reactivity. Conversely, large amounts of hydrogen lost from a reactor core over a period of time may decrease the reactivity to the point that a premature shutdown and re-fueling is necessary. Therefore, a more detailed study of the residual hydrogen and other gaseous impurities in fuel rods after carbonization and methods of control has been undertaken.

Several fuel rods were carbonized under vacuum at increased carbonization time and temperature. The hydrogen content of each whole rod (including the fuel particles) was determined; the results are given in Table 3.4. These processing conditions appear to result in a lower residual hydrogen in the fuel rods.

The vacuum-fusion method of analysis was used to determine the hydrogen in the initial samples. With this method, the samples are heated by induction to high temperatures in a closed vacuum system and the quantity of gas given off is measured after passing through selective traps. Although low concentrations of pure hydrogen gas can be measured accurately by this technique, other hydrogen-containing gases, such as water vapor and hydrocarbons, are condensed in the traps and not measured. If significant quantities of these are present, the total hydrogen determination for the sample may much too low.

Another method by which hydrogen content may be determined is the total combustion of the sample and measurement of the H_2O given off. Although this method is not as sensitive to small quantities of hydrogen, the total hydrogen content of a sample, no matter what the form, can be measured.

Duplicate samples from an 11-in. fuel rod (LTI TRISO particles and D-38 matrix carbonized for 1 hr at 1000°C under vacuum) were analyzed by both the vacuum fusion and combustion methods. The hydrogen content as determined by the vacuum fusion method was 294 ppm; that by the combustion method was 600 ppm. This would indicate that significant amounts of hydrogen are either retained in the sample or present in released materials that are not detected by the vacuum fusion method. Therefore, for future hydrogen determinations, either the combustion method will be used or the possible difference will be taken into account when vacuum fusion is used.

Matrix Overcoating Process

Work has been initiated on an alternative method of fabricating fuel rods based on overcoating fuel particles with a dry resin-graphite powder mix. Highly successful applications of this type of process in making fuel

Table 3.4
HYDROGEN CONTENT OF FUEL RODS CARBONIZED AT 1000°C
WITH VARIOUS PROCESSING CONDITIONS^a

Time at Temp (hr)	Atmosphere	H Content (ppm)	Equivalent H in Core (lb)
1	Vacuum	175	22.0
1	Nitrogen	300	37.5
4	Vacuum	170	21.4
4	Nitrogen	220	27.5

^a 1-in.-long fuel rods containing fissile- and fertile-size LTI TRISO-coated fuel particles in D-38 matrix cured for 1 hr at 100°C and carbonized as noted.

compacts have been reported (Refs. 1, 2). Generally, the particles to be coated are tumbled in a rotating drum while solvent is dispersed in an air stream and a dry resin-graphite powder mix is dropped into the solvent-air mixture. This effectively "sprays" the resin-matrix mix onto the tumbling particles and builds up the desired matrix overcoat. When the overcoated particles are packed in a fuel rod mold and heat applied (160°C for 1 hr), the resin in the matrix softens, fuses together, and expands to form a homogeneous matrix around the particles. Several 1-in. rods have been fabricated by this method and then successfully carbonized by the usual procedure.

To date 5, 18, 20, and 24 wt-% applications of overcoat matrix material have been applied to coated fuel particles. The overcoat material formulation consists of 75 wt-% 11078 phenolic resin and 25 wt-% GP-38 graphite powder. The 24 wt-% overcoat is approximately 100- μ thick. Several 1-in. rods were prepared from each by restraining the overcoated particles in a suitable mold and curing at 180°C. The results are given in Table 3.5.

Additional 1-in. rods were prepared by subjecting the 24 wt-% overcoated particles to various bonding pressures during the curing stage. The results are compiled in Fig. 3.6. All rods appeared homogeneous and strong. Attempts were also made to fabricate 11-in.-long fuel rods by this method. A fairly good rod was prepared by using pressure to pack the overcoated particles in the mold (i.e., loading a portion of the particles in the preheated mold and pressing in place) and then applying a final bonding pressure of 10 to 50 psi during curing. The rod appeared strong and solid, but different strata of particles were apparent. The first trials at making 11-in. rods by loose packing of particles with restraint only during curing did not appear too successful. Low particle packing fractions, large voids, and nonhomogeneity were some of the problems encountered. However, it is felt that these problems may be overcome by increasing the foaming action of the matrix material. Present efforts are directed toward increasing both the foaming action of the matrix and the curing rate.

Fig. 3.6--Variation in packing fraction with packing pressure for 1-in. fuel rods containing 600- to 800- μ m HTI coated particles with 24 wt-% overcoat

Table 3.5
RESULTS OF MATRIX OVERCOATING STUDIES

Sample No.	Overcoat (wt-%)	Particle Packing Fraction (vol-%)	Appearance after Curing 1 hr, 180°C
1	5	60	Weak, fell apart when shaken in glass vial
2	18	37	Strong, multiple voids and inhomogeneities, extensive shrinking
3	20	41	Strong, multiple voids and inhomogeneities
4	24	24	Strong, multiple voids, appeared reasonably homogeneous

Thermal Conductivity of Bonded Beds

Thermal conductivity measurements were conducted to evaluate a bonded bed that is representative of the current fuel materials. A blend of equal parts by volume of fissile and fertile particles were bonded by injection of matrix formulation D-38. Using a mold volume of 64.6 cm³, the coated particle packing fraction was calculated to be 61.4%.

Conductivity measurements were made using a comparative method (Refs. 3, 4). The results are shown in Fig. 3.7 along with curves for previously tested samples taken from Ref. 3. The present sample retained a high conductivity even though the matrix density was low. The retention of high conductivity may have been due to the use of a graphite filler instead of charcoal.

An analysis was performed using the bed conductivity model of Deissler and Eian (Ref. 5) to establish the relative sensitivity of bed conductivity to changes in particle and matrix conductivity. The results shown in Fig. 3.8 reveal that changes in particle and matrix conductivity have very nearly the same influence on bed conductivity. In addition, it is noted that each curve has a slope less than 1 (i.e., an increase in either particle or matrix conductivity results in a smaller change in bed conductivity). These are indications that irradiation-induced changes in bed conductivity should not be large.

Graphite Development

The objective of this program is the evaluation of various conventional needle-coke and a few nominally isotropic graphites for use in the PSC reactor core. The graphites presently under examination have been obtained from the three principal U. S. Vendors and are being evaluated with respect to purity, thermal properties, mechanical properties, and cost. These graphites currently are being evaluated for use as a moderator and structural lattice

Fig. 3.7--Thermal conductivity of a porous bonded bed of TRISO-II-coated particles compared with previous results on beds of BISO particles

Fig. 3.8--Analytically derived thermal conductivity of a bonded bed for simple cubic packing

for fuel elements and as the surrounding neutron-reflecting material. The graphites that have been under consideration for the above applications are as follows: H-327; H-328, 9567, RC4, and TS-688.

Evaluation of full-size H-327 graphite logs (pilot lot No. 340 produced in 1966) has been completed and the data are being compiled. Five logs from this lot were sampled for mechanical properties and three for thermal/physical properties. The samples were extracted as core-drilled rods with either 0.50-in. diameters for Type III tensile bars or 1-3/8-in. diameters for Types II and IV tensile bars (see Fig. 3.9). Specimens for thermal/physical tests were taken at various locations on the cored rods, adjacent to the tensile bars.

Some minor variations were observed throughout two of these H-327 logs in thermal expansion, thermal conductivity, and bulk density as a function of location; however, very little variation was noted for ultimate compressive strength. The uniformity of properties within the logs appears satisfactory.

A tensile testing series* established the following points with respect to test procedures:

1. Replacement of older jigs and fixtures used in preparing the glued-end-cylinder specimens, as well as an improved alignment during testing, raised the level of tensile fracture results observed in parallel (axial) specimens by about 5% while reducing the extent of experimental scatter by about 19%. This specimen type, which has been preferred at Gulf General Atomic, is identified as Type III and is shown in Fig. 3.9.
2. Observations on the two methods of fabricating glued-end-cylinder specimens, Types III and IV, did not show consistent changes in levels of fracture stress nor in extent of data scatter when Type IV results were compared with those for Type III.
3. A square-cross-section dog-bone specimen with pin-ended gripping was shown to give somewhat higher tensile fracture results (roughly 9% to 17% higher) and less data scatter than was obtained with the present Gulf General Atomic preferred specimen. This dog-bone type of specimen is identified in Fig. 3.9 as Type II. Practical considerations of blank size and of preparation cost still favor the use of Type III specimens.

A series of comparative tensile tests has been initiated to standardize testing procedures and sample configurations. A comparison of results obtained at two cross-head rates (0.02 and 0.005 in./min) revealed a significant effect of strain rate on the resulting ultimate tensile stress (UTS) of H-327 graphite. UTS values were found to drop about 12% when the cross-head rate was increased by a factor of four to 0.02 in./min.

*This test series is about 75% completed, with only the Type I specimens untested to date (see Fig. 3.9).

Fig. 3.9--Various tensile bar configurations under evaluation and comparison

Since some Type III tensile bars (as well as Type II bars) were observed to fracture within the uniform-gage section near one end or the other, the test results were examined in order to determine what effect these samples exercised on the over-all results for their group. Statistical determinations of mean values for samples breaking within one diameter (0.50 in.) from an end of the Type III bar and for the remainder of the samples indicated that there were no significant differences in the mean values, ranges, or standard deviations.

Conglomerate values for the above properties have been determined and indicate that all of the logs examined would have been acceptable when sampled in accordance with Gulf General Atomic specifications. The distribution of lot-mean UTS values in parallel (axial) Type III tensile bars as a function of radial distance from log axis is presented in Fig. 3.10, surrounded by a band of $\pm 1\sigma$ limits. These results exhibit a strong UTS gradient, decreasing from the log-edge to midradial locations, with a rapidly decreasing gradient across the remaining distance to the log axis.

Within the central third of the cross section of each H-327 log, a network of fissures was observed. Similar fissures were subsequently detected in grades 9567 and TS-814,* but on a finer scale (very narrow gaps). As a result of preliminary and cursory analysis of these structural features, it appears that radiation effects on a fuel block containing such a central fissured region should not be detrimental to the utility of the block. A second pilot-production log of H-327 logs is being processed. A structural examination of these logs will establish whether or not these production logs also contain central regions with fissures on a finer scale than was detected in the first lot.

Computer programs for a graphite data storage-retrieval system and for data analysis are being written and debugged. The data input forms for card punching have been evaluated and are being utilized to receive all pertinent graphite data presently available for the initial inventory of the storage system.

Control Material

Control Rod Absorber Material. A study was begun to determine the effects of B_4C concentration and bake temperature on strength. The warm-press and bake technique was used to produce modulus of rupture specimens 0.1 in. by 0.2 in. by 0.6 in. The design boron concentrations were 0, 0.4, 0.5, and 0.6 g B/cm³ (the reference boron content is 0.63 g B/cm³). Chemical analysis of the material is incomplete. After pressing at 725°C, the specimens were baked 2 hr at either 1000° or 2000°C. Fabrication of specimens is complete, and modulus of rupture measurements for specimens with axes parallel and perpendicular to the pressing direction will be measured.

Boronated Reflector Graphite. A portion of the reflector graphite will be boronated to obtain a boron concentration between 1 and 2 wt-%. The boronated reflectors will operate at temperatures between 400° and 825°C and will

*Grade TS-814 is an improved EGCR grade TS-688 graphite with additional impregnations.

Fig. 3.10--Summary of UTS values (parallel) in five 18-in.-diam logs of H-327 graphite (lot 340)

accumulate a neutron exposure of about 1.5×10^{21} n/cm² thermal ($E < 2.38$ ev) and 0.2×10^{21} fast ($E > 0.18$ Mev) during a 30-year period. An indication of the magnitude of irradiation-induced changes that are possible in the boronated reflectors can be obtained from examination of irradiation data for graphite containing 5 and 7 wt-% boron which are reported in Ref. 6. Table 3.6 is a summary of irradiation data for graphite containing 5 and 7 wt-% boron after a neutron exposure approximating 30 years in the PSC reactor.

Table 3.6

PROPERTIES OF IRRADIATED GRAPHITE CONTAINING 5 AND 7 WT-% BORON^aNeutron exposure, n/cm²:Thermal, $E < 2.38$ ev 1.5×10^{21} Fast, $E > 0.18$ Mev 0.2×10^{21}

Exposure temperature, °C 370-500

Dim. change during irradi., %:

Perp. to extrusion 0.4-0.8 expansion

Para. to extrusion 0.4-0.6 contraction

Thermal conduc., cal/sec-cm²-°C^b 0.02-0.04Compressive strength, psi^c 7000-9000Coeff. of thermal exp., °C⁻¹ ^d 2×10^{-6}

^aNeutron exposure equivalent to 30 years in PSC boronated reflector region (data derived from Ref. 6).

^bInitial thermal conductivity approximately 0.2 cal/sec-cm²-°C.

^cInitial compressive strength 4500 psi.

^dInitial CTE 2×10^{-6} °C⁻¹.

The major differences between the PSC material and the irradiated boronated graphite arise from the fact that the bake temperature of the PSC material will be much lower (1400° to 2000°C compared to about 2450°C) and the boron content will also be lower. The utilization of a lower bake temperature and boronation level should result in less of the graphite matrix being saturated with boron. Consequently, a lower level of irradiation effects should be experienced in the PSC reactor than were reported in Ref. 6. In addition, the effects of irradiation were generally less pronounced at 500°C than at 370°C, so material irradiated at 800°C might show somewhat less change. Therefore, the assumption is made here that the irradiation results reproduced in Table 3.6 represent an upper limit for irradiation-induced changes in the boronated graphite reflectors of the PSC HTGR. Briefly, the data show that the changes in dimension and thermal expansion coefficient may be relatively small, but thermal conductivity may decrease by as much as a factor of 10, while the compressive strength increases by a factor of 1.5 to 2.0.

Irradiation Testing

Irradiation experiments are being conducted to test BISO- and TRISO-coated particles under design irradiation conditions. These experiments are intended to guide the selection of fuel materials that will meet the performance requirements of the PSC reactor. There are three experiments currently in progress that are proof-tests intended to demonstrate the irradiation stability of BISO-coated particles under reactor conditions. Post-irradiation examination of one irradiation test of TRISO-coated particles has been completed. Two capsules containing TRISO-coated fuel are at the ETR awaiting insertion in the next cycle, and another capsule containing TRISO-coated fuel is being fabricated. Irradiation tests of fuel rods containing TRISO-coated particles are also planned, and the first fuel rod capsule is being fabricated. All experiments are being or will be conducted at the PSC maximum fuel temperature to full design fuel burnup and to a significant fraction of the integrated fast-neutron dose.

Graphite irradiation experiments in this program have been directed toward obtaining data on conventional graphites over the temperature range and to the lifetime dose of the reactor. Physical properties and dimensional changes have been and are being measured and the results analyzed to aid in the selection of fuel element graphite. A series of capsule experiments is in progress to irradiate conventional graphites in the range 550° to 1100°C to a maximum dose of about 8×10^{21} n/cm² ($E > 0.18$ Mev).

Capsule P17. Capsule P17 is the first proof test of BISO-coated particles. The coated particle samples were designed in accord with current models of coated particle and pyrolytic carbon irradiation behavior to give a range of values of merit factor (Ref. 7) and strain rate (Ref. 8). Both BISO-HTI and ISO-LTI coatings are included. The desired merit factors and strain rates were obtained by systematically varying the structure and thickness of the outer isotropic layer while the thickness and density of the inner buffer layer were held constant. Most of the coating types are being tested in triplicate on cores with different Th:U ratios to ensure that at least one sample will be close to the design burnup despite ETR flux variations. All samples are being tested on fissile size cores. Detailed descriptions of the coated particle samples were given in a previous quarterly report (GA-8038).

The fuel temperatures in this capsule were programmed to simulate the thermal history of a typical maximum temperature region in the PSC core. The temperatures of these regions in the core will increase from 1100° to 1300°C during the first year (to about 7% FIMA), drop abruptly to 1100°C at the start of the second year due to the partial refueling, and then gradually decrease from 1100° to 1000°C over the remaining five years (to 20% FIMA). The capsule was irradiated at 1300°C to about 13% FIMA, at 1100°C to 20% FIMA, and at 1000°C for those samples that exceeded 20% FIMA.

The P17 capsule completed five cycles of irradiation in the 113NE core position of the ETR. The capsule operated satisfactorily with near-design end-of-life fuel temperatures of 1080° to 1130°C. Only two of the original 15 fuel thermocouples were still operative at the end of the last cycle, but the fuel bed temperatures could be estimated from previously established

correlations with backup reference thermocouples located outside the fuel bed. The fuel reached estimated burnups of 27%, 22%, and 19% FIMA (for the three different Th:U ratios on fissile particles) with an estimated fast-neutron dose of 5×10^{21} n/cm². The capsule is now being prepared for postirradiation examination.

Capsule P18. Capsule P18 is the second proof test of BISO-coated particles. It is a longer-term test (seven ETR cycles) than capsule P17 of similar samples designed to achieve a higher fast-neutron dose. The dose will be about 7.5×10^{21} n/cm² ($E > 0.18$ Mev), which is very close to the full PSC maximum dose. Some of the samples have fuel cores with different Th:U ratios from those of P17 so that the burnups will still center around 20% FIMA (PSC maximum) despite the longer exposure. As in capsule P17, the fuel temperatures are also programmed to simulate the temperature history of a typical PSC maximum temperature core region. A detailed description of the coated particle samples in capsule P18 was given in a previous quarterly report (GA-8038).

The P18 capsule has completed five cycles of irradiation in the ETR in the I13SE core position and is in its sixth cycle. The estimated fuel burnups to date are 23%, 19%, and 16% FIMA, with an estimated fast-neutron dose of 6.1×10^{21} n/cm². The cell containing specimens with 23% FIMA is operating near 1000°C, as scheduled. The remaining cells containing specimens with burnup less than 20% FIMA are operating at 1000° to 1050°C rather than the programmed 1100°C.

Capsule P19. Capsule P19 is the first P-capsule proof-test of BISO-coated particles prepared in large-scale (production) equipment. Several different isotropic layer density and anisotropy factor combinations are being tested for both HTI and LTI to determine the most economical combination that will survive HTGR design irradiation conditions. A detailed description of the coated particle samples in P19 was given in the previous quarterly report (GA-8600).

Capsule P19 was designed to achieve full PSC fuel burnup of 20% FIMA and a fast-neutron dose of 6×10^{21} n/cm² at the maximum PSC fuel temperature of 1300°C. The fuel temperatures will be programmed to simulate the thermal history of a typical HTGR maximum temperature core region.

The capsule is now in its first cycle in the ETR (core position I13NW). Three cells are operating at design temperature: one at 900°C and two at 1300°C (cells 1, 2, and 3, respectively). The other two cells, however, are operating between 1100° and 1150°C rather than at the design temperature of 1300°C. The burnup is now about 4% FIMA at a fast-neutron dose of 0.5×10^{21} n/cm².

Capsule P13J. Capsule P13J was a test of TRISO-coated (BISO-SiC) particles designed to determine the optimum coating geometry for this type of particle and to evaluate the effect of irradiation temperature on SiC coating stability. All samples in this capsule had low-temperature isotropic (LTI) pyrocarbon outer coatings over the SiC. P13J was examined after four ETR cycles, and the results of the examination were reported in the previous quarterly report (GA-8600).

During this report period the fast-neutron dose and burnup measurements were completed for each sample. These latest data are shown in Table 3.7, which is the final summary table for the P13J experiment. There was considerable variation in exposure and burnup as a function of location in the capsule, and in all cases the measured value was less than the previously estimated values, which were 4.0×10^{21} n/cm² (fast) and 19% FIMA.

The conclusion drawn from the P13J capsule experiment, together with results from the earlier P13F and P13H experiments, was that TRISO-coated particles with a wide range of coating geometries demonstrated very good stability at low, medium, and maximum PSC core temperatures to 65% to 85% of full PSC burnup and to 40% to 50% of the full PSC fast-neutron dose.

Capsule P20. Capsule P20 will be used to evaluate variations in the TRISO coating design and will demonstrate the stability of TRISO-coated fuel after exposure to 50% to 75% of full PSC fuel burnup. All of the coatings are of either the TRISO-I or TRISO-II design. The TRISO-I coating design consists of a buffer, an SiC layer, and an outer isotropic PyC layer. The TRISO-II design differs from the TRISO-I by having an additional isotropic PyC layer between the buffer and the SiC layer.

In the P20 capsule all coatings are on 200- μ m fissile fuel particles and have buffers between 45 and 50 μ m thick. In the TRISO-II coatings the inner isotropic LTI coating is a constant 25 μ m in thickness. The experimental design variations are in the thickness, density, and deposition process (LTI versus HTI) of the outer PyC layer, and the thickness of the SiC layer. In addition, one sample each of TRISO-I and -II coatings have outer isotropic layers doped with Si to retard changes in anisotropy. The selection of all coating parameters was directed toward improving the long-term irradiation stability of the coatings by optimizing the mechanical stress relationships in the coating components. The Kaae analytical model (Refs. 9, 10) was used extensively in guiding selection of coatings to be included in the experiment. A detailed description of each sample, the design irradiation conditions, and the experimental objectives of each series are shown in Table 3.8.

The capsule was designed to achieve in the samples a full PSC fuel burnup of 20% FIMA and a fast-neutron dose ranging from 4×10^{21} to 6×10^{21} n/cm² at temperatures of 1300° or 1000°C. The 1300°C cells will have temperatures lowered to 1100°C at about 10% FIMA to simulate the thermal history of a typical HTGR maximum-temperature core region. Capsule P20 was inserted in cycle 96 toward the end of May.

Capsule P21. Capsule P21 will evaluate and demonstrate the stability of TRISO-I and TRISO-II coated particles similar to those in P20 to a greater neutron exposure (75% to 100% of full PSC fast-neutron exposure) and full PSC fuel burnup. Coatings were deposited on 200- μ m fissile fuel particles. Two buffer coatings are about 40 μ m thick, and the remainder are about 50 μ m thick. The other experimental design variations are the thickness of the inner LTI PyC, of the SiC layer, and of the outer isotropic PyC, as well as deposition process (LTI versus HTI), density, and coating facility (production versus laboratory equipment). In addition to the above sample series, one sample is included that has a TRISO-I coating with an outer LTI coating doped with silicon to retard irradiation-induced changes in anisotropy.

Table 3.7
DESCRIPTION AND POSTIRRADIATION EXAMINATION OF COATED PARTICLES TESTED IN CAPSULE P13J
(Four-cycle ETR irradiation)

Lot No.	Type	Coating							Particle	Irradiation Conditions				Postirradiation Examination		Analytical Parameters				Remarks			
		Buffer		SIC	Isotropic			Total Thickness (μ)		Burnup (± FIWA)			Temperature (°C)			Fast Dose (10 ⁻²¹ nvt)	Micro Exam ^a (% Fail)	Met. Exam ^b (% Fail)	Merit Factor ^c		Shrinkage Rate ^d (1/10 ⁻²¹ nvt)		
		Thickness (μ)	Density (g/cm ³)	Thickness (μ)	Thickness (μ)	Type ^e	Density (g/cm ³)			Anisotropy Factor	Type ^f	Size (μ)	Th:U Ratio	Avg ^g	Max				SOL ^h			SiC	ISO PyC
3592-35E	SIC barrier	39	1.38	15	10	LTI	(1)	(1)	84	(Th,U)C ₂	150-250	2:1	17	1280	1350	1180	3.6	1	4	700	(1)	(1)	SiC load-bearing series
3592-37E	SIC barrier	39	1.38	25	10	LTI	(1)	(1)	74	(Th,U)C ₂	150-250	2:1	18	1300	1340	1200	3.6	0	0	1100	(1)	(1)	
3592-45E	SIC barrier	39	1.38	37	8	LTI	(1)	(1)	84	(Th,U)C ₂	150-250	2:1	16	1280	1330	1180	3.6	0	0	1650	(1)	(1)	
3592-47E	SIC barrier	39	1.38	10	31	LTI	2.03	(2)	80	(Th,U)C ₂	150-250	2:1	16	1180	1300	1110	3.3	0	0	450	550	-1	PyC load-bearing series
3592-53E	SIC barrier	39	1.38	10	32	LTI	2.03	1.23	101	(Th,U)C ₂	150-250	2:1	16	1290	1350	1200	3.3	0	0.2	450	900	-2.5	
3592-69E	SIC barrier	51	1.21	29	10	LTI	(1)	(1)	90	(Th,U)C ₂	150-250	2:1	17	1290	1340	1200	3.8	0	0	2000	(1)	(1)	SiC load-bearing series with thicker buffers
3592-71E	SIC barrier	51	1.21	35	10	LTI	(1)	(1)	96	(Th,U)C ₂	150-250	2:1	17	1310	1370	1240	3.8	0	0	2350	(1)	(1)	
3592-35E	SIC barrier	39	1.38	15	10	LTI	(1)	(1)	64	(Th,U)C ₂	150-250	2:1	17	1000	1080	1000	3.1	6	9	850	(1)	(1)	Intermediate-temperature SiC load-bearing series
3592-37E	SIC barrier	39	1.38	25	10	LTI	(1)	(1)	74	(Th,U)C ₂	150-250	2:1	18	950	1050	950	3.1	0.7	0	1400	(1)	(1)	
3592-45E	SIC barrier	39	1.38	37	8	LTI	(1)	(1)	84	(Th,U)C ₂	150-250	2:1	16	980	1050	980	3.1	0	0	2000	(1)	(1)	
3592-35E	SIC barrier	39	1.38	15	10	LTI	(1)	(1)	64	(Th,U)C ₂	150-250	2:1	13	600	650	600	2.7	0.2	0	1100	(1)	(1)	Low-temperature SiC load-bearing series
3592-37E	SIC barrier	39	1.38	25	10	LTI	(1)	(1)	74	(Th,U)C ₂	150-250	2:1	12	610	670	610	2.7	0	0	1800	(1)	(1)	
3592-45E	SIC barrier	39	1.38	37	8	LTI	(1)	(1)	84	(Th,U)C ₂	150-250	2:1	14	630	690	630	2.7	0	0	2650	(1)	(1)	
3583-67E	BISO(1300)	51	1.21	—	65	LTI	2.04	1.03	116	(Th,U)C ₂	150-250	2:1	17	1280	1340	1190	3.8	0	0	—	1650	-1	BISO
3592-73E	BISO(2000)	80	1.11	—	38	HTI	2.05	1.04	118	(Th,U)C ₂	150-250	1.6:1	17	1280	1330 ⁱ	1190	3.3	0	0	—	1610	-0.4	

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

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

^c Average of maximum temperatures.

^d End-of-life maximum temperatures.

^e Determined by microscopic examination of ~2000 particles.

^f Determined by metallographic examination of 150 to 500 particles.

^g Calculated by method of Schwartz (GA-7262) using end-of-life conditions and assuming either the SiC or the isotropic PyC to be the load-bearing layer.

^h Shrinkage rate of isotropic PyC coating in parallel direction at total strain of ~2%. Estimated from data of Bokros and Schwartz (GA-7700).

ⁱ Not determined.

Table 3.8
DESCRIPTION OF LABORATORY-PREPARED COATED PARTICLES BEING TESTED IN CAPSULE P20

Lot No.	Type ^b	Coating ^a										Particle			Design Irradiation Conditions			Remarks
		Buffer		Isotropic (LTI)		SiC		Isotropic			Total Thickness (um)							
		Thickness (um)	Density (g/cm ³)	Thickness (um)	Density (g/cm ³)	Thickness (um)	Estimated Density (g/cm ³)	Thickness (um)	Density (g/cm ³)	BAF								
												Type	Size (um)	Th:U Ratio	Burnup (% FIMA)	Temp (°C)	Fast Dose (10 ²¹ n/cm ²)	
3923-85	TRISO-I LTI	46	1.15	--	--	20	3.17	48	1.93	1.06	114	(Th,U)C ₂	200	2.25	20	1300	5	TRISO-I LTI series
3923-85	TRISO-I LTI	46	1.15	--	--	20	3.17	48	1.93	1.06	114	(Th,U)C ₂	200	2.25	20	1000	5	
3923-99	TRISO-I LTI	46	1.15	--	--	15	3.17	47	1.96	1.00	108	(Th,U)C ₂	200	2.25	20	1300	5	
3923-97	TRISO-I LTI	46	1.15	--	--	10	3.17	49	1.95	1.03	105	(Th,U)C ₂	200	2.25	20	1300	5	
3923-127	TRISO-I HTI	46	1.15	--	--	20	3.17	47	1.96	1.00	113	(Th,U)C ₂	200	2.25	20	1300	6	TRISO-I HTI series
3923-131	TRISO-I HTI	46	1.15	--	--	15	3.17	54	1.86	1.00	115	(Th,U)C ₂	200	2.25	20	1300	6	
3923-137	TRISO-I HTI	46	1.15	--	--	10	3.17	50	1.88	1.00	106	(Th,U)C ₂	200	2.25	20	1300	6	
3923-101	TRISO-II LTI	50	(c)	25	(c)	15	3.17	29	1.98	1.10	117	(Th,U)C ₂	200	2.25	20	1300	4	TRISO-II LTI series
3923-87	TRISO-II LTI	50	(c)	25	(c)	20	3.17	30	1.95	1.01	122	(Th,U)C ₂	200	2.25	20	1300	4	
3923-87	TRISO-II LTI	50	(c)	25	(c)	20	3.17	30	1.95	1.01	122	(Th,U)C ₂	200	2.25	20	1000	5	
3923-141	TRISO-II HTI	50	(c)	25	(c)	15	3.17	31	1.84	1.01	119	(Th,U)C ₂	200	2.25	20	1300	6	TRISO-II HTI series
3923-149	TRISO-II HTI	50	(c)	25	(c)	21	3.17	32	1.86	1.03	127	(Th,U)C ₂	200	2.25	20	1300	6	
3923-149	TRISO-II HTI	50	(c)	25	(c)	21	3.17	32	1.86	1.03	127	(Th,U)C ₂	200	2.25	20	1000	5	
4032-149	TRISO-I LTI (Si-doped)	46	1.15	--	--	15	3.17	46	2.08	1.05	107	(Th,U)C ₂	200	2.25	20	1300	6	Si-doped outer isotropic layer series
4042-7	TRISO-II LTI (Si-doped)	50	(c)	25	(c)	15	3.17	32	2.09	1.02	120	(Th,U)C ₂	200	2.25	20	1300	4	

^a TRISO-I denotes a coating with the following sequence: Buffer, SiC, LTI or HTI isotropic PyC. TRISO-II denotes a coating with the following sequence: Buffer, LTI isotropic PyC, SiC, LTI or HTI isotropic PyC.

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

^c Not determined.

There is also a sample of TRISO-II-coated particles included that was taken from the same coating lot (3332-101E) that showed low fission-product release after exposure in capsule P13F to 2.2×10^{21} n/cm² ($E > 0.18$ Mev), 11% FIMA. Capsule P21 will be a more severe irradiation test of these particles than was capsule P13F.

As with capsule P20, the selection of coatings in P21 was guided by the Kaae model (Refs. 9, 10) in an attempt to optimize the stress relationship among coating components.

A detailed description of each sample, the design irradiation conditions, and the experimental objectives of each series are shown in Table 3.9.

The capsule was designed to achieve full PSC fuel burnup of 20% FIMA and a fast-neutron dose of 6×10^{21} to 8×10^{21} n/cm² at temperatures of 1300° or 1000°C. The temperature of cells initially at 1300°C will be lowered to 1100°C at about 10% FIMA burnup to simulate the thermal history of a typical HTGR maximum-temperature core region. Capsule P21 was inserted in cycle 96 toward the end of May.

Capsule P22. Capsule P22 will be a proof test of TRISO-coated fertile and fissile particles produced both in laboratory and in production equipment. The test conditions will be a fast-neutron exposure 8×10^{21} n/cm², fuel burnups of 20% FIMA on the fissile particles and 7% FIMA on the fertile particles, and initial fuel temperatures of 900°, 1100°, and 1300°C.

All fuel samples have been coated and partially characterized. The capsule is now being fabricated; insertion in the ETR is scheduled for cycle 97 in mid-July.

Fuel Rod Capsules. Fuel rod irradiation experiments in the ETR are being planned to evaluate the stability of the fuel rods and the effects of the fuel rod matrix on coated particle stability at high fast-flux exposures. The coated particles tested to date have been loose particles, and the irradiation program is designed to guide the development of coated particles that can survive full PSC exposure conditions. Bonding the particles together into a fuel rod may impart additional stresses to the coatings as the carbonaceous matrix undergoes dimensional changes during fast-neutron irradiation. These experiments will permit a study of the effects of irradiation on the whole rod and the matrix material and on particle-matrix interactions.

Capsule FR-1 will test 1/2-in.-diameter, 1-in.-long fuel rods to full burnup of the fissile particles at 1260° or 900°C to high fast-flux exposures. Blends of fissile and fertile TRISO-coated particles will be used. Loose fissile and fertile coated particles will also be tested with each fuel rod so that their performance can be compared with those in the rods.

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

Table 3.9
DESCRIPTION OF COATED PARTICLES BEING TESTED IN CAPSULE P21

Lot No.	Coating ^a												Particle			Design Irradiation Conditions				Remarks
	Type ^b	Pre-prepared ^c	Buffer		Isotropic (LTI)		SiC		Isotropic			Total Thickness (μm)								
			Thickness (μm)	Density (g/cm ³)	Thickness (μm)	Density (g/cm ³)	Thickness (μm)	Density (g/cm ³)	Thickness (μm)	Density (g/cm ³)	BAF									
3923-63	TRISO-I LTI	Lab	54	1.03	--	--	20	(d)	46	1.90	1.02	120	(Th,U)C ₂	200	2.75	20	1300	7	TRISO-I LTI series	
3923-95	TRISO-I LTI	Lab	54	1.03	--	--	14	(d)	50	1.91	1.01	119	(Th,U)C ₂	200	2.75	20	1300	7		
4000-886	TRISO-I LTI	Prod	38	1.37	--	--	19	(d)	41	1.98	1.13	113	(Th,U)C ₂	200	2.75	20	1300	8		
4000-886	TRISO-I LTI	Prod	38	1.37	--	--	19	(d)	41	1.98	1.13	113	(Th,U)C ₂	200	2.75	20	1000	7		
3923-71	TRISO-I HTI	Lab	54	1.03	--	--	20	(d)	48	1.98	1.00	122	(Th,U)C ₂	200	2.75	20	1300	8	TRISO-I HTI series	
3923-135	TRISO-I HTI	Lab	49	1.26	--	--	14	(d)	49	1.90	1.00	112	(Th,U)C ₂	200	2.75	20	1300	8		
3923-113	TRISO-II LTI	Lab	50	(e)	10	(e)	18	(d)	29	1.95	1.01	109	(Th,U)C ₂	200	2.75	20	1300	6	TRISO-II LTI series	
3923-113	TRISO-II LTI	Lab	50	(e)	10	(e)	18	(d)	29	1.95	1.01	109	(Th,U)C ₂	200	2.75	20	1000	7		
3923-103	TRISO-II LTI	Lab	50	(e)	20	(e)	18	(d)	30	1.94	1.03	115	(Th,U)C ₂	200	2.75	20	1300	6		
4000-889	TRISO-II LTI	Prod	50	(e)	20	(e)	17	(d)	48	1.92	1.07	133	(Th,U)C ₂	200	2.75	20	1300	8		
4000-889	TRISO-II LTI	Prod	50	(e)	20	(e)	17	(d)	48	1.92	1.07	133	(Th,U)C ₂	200	2.75	20	1000	7	TRISO-II HTI series (with low-density HTI)	
3923-111	TRISO-II HTI	Lab	50	(e)	20	(e)	18	(d)	33	1.89	1.00	118	(Th,U)C ₂	200	2.75	20	1300	6		
3923-139	TRISO-II HTI	Lab	50	(e)	20	(e)	20	(d)	29	1.84	1.01	119	(Th,U)C ₂	200	2.75	20	1300	8		
4042-3	TRISO-I LTI (Si-doped)	Lab	54	1.03	--	--	14	(d)	50	2.11	1.03	118	(Th,U)C ₂	200	2.75	20	1300	7	Si-doped outer iso-tropic layer	
3332-101E	TRISO-II HTI	Lab	48	(e)	10	(e)	13	3.15	40	(e)	1.01	111	(Th,U)C ₂	200	3.00	20	1300	8	Repeat sample from P13F capsule	

^a TRISO-I denotes a coating with the following sequence: Buffer, SiC, LTI or HTI isotropic PyC. TRISO-II denotes a coating with the following sequence: Buffer, LTI isotropic PyC, SiC, LTI or HTI isotropic PyC.

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

^c Prod denotes particles coated in production equipment; lab denotes particles coated in laboratory equipment.

^d Density estimated as 3.17 g/cm³.

^e Not determined.

to monitor centerline fuel temperatures. A thermocouple will also be included to each loose coated particle sample and in the graphite around the fuel rod. Three different matrix materials (D-25, D-38, and D-24) and two particle types (LTI TRISO-II and HTI TRISO-II) have been selected as composition variables for the rods.

Capsule FR-1 is scheduled to begin irradiation in the ETR in cycle 97 (July), and it is planned for a fast-neutron dose of 4 to 6×10^{21} n/cm² ($E > 0.18$ Mev).

Capsule FR-2 and possibly an FR-3 will test similar samples to a higher fast-neutron dose of 6 to 9×10^{21} n/cm². Design and fabrication of these capsules is under way; irradiation is scheduled to begin in cycle 98 in August.

Capsule G13. Capsule G13 contains needle-coke graphite grades H-327, 9567 (Nuclear II purity), and NC8 (EGCR), as well as nominally isotropic grade RC4. This capsule, now in its fifth ETR cycle, was designed to irradiate specimens from large-diameter extrusions of PSC candidate graphites in the temperature range 600° to 1050°C to a maximum dose of 5 to 6×10^{21} n/cm² ($E > 0.18$ Mev). Approximately 70% of this maximum dose has been accumulated during the first four cycles and the instrumentation is still functioning satisfactorily.

Evaluation of G13 control samples by the sonic-resonance method to measure elastic modulus (and, it is hoped, shear modulus and Poisson's ratio) is under way. All room-temperature measurements for elastic modulus by this method have been completed, as well as over half of those at temperatures to 1000°C . Room-temperature measurements for shear modulus have begun, after an improvement was made in the detection of specimen response to this mode of excitation. Preliminary measurements of the elastic modulus between 22° and 1000°C revealed a slight increase (about 5%) in the perpendicular direction and a small decrease (about 7%) in the parallel direction for grade H-327 graphite.

A special tensile-test fixture, referred to as a "theta ring" (Ref. 11), is being evaluated. If the results are satisfactory, the fixture will permit testing (at room temperature only) of sub-size graphite tensile specimens, such as samples from Capsule G13 and similar capsules, by loading them in the crossbar position within a reusable high-strength steel ring.

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

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

In-pile Release from P13H Particles. The releases of fission products from TRISO-coated particles during the P13H capsule irradiation have been determined using gamma spectroscopy and radiochemistry. The experimental procedure used to determine the in-pile fission-product release was similar to that used previously to analyze the in-pile release from the P13F capsule. The graphite crucibles and thimbles were gamma-counted on the lithium-drifted germanium detector to determine the releases of cesium, cerium, europium, and barium. The thimbles and crucibles were then dissolved and analyzed for strontium. A new computer program was devised to process the raw counting data and calculate the fractional release of metallic fission products during the irradiation. This calculation has been improved over that used previously with the P13F capsule particles by the addition of a term that corrects for power fluctuations and down time during the irradiation. The correction for power changes is given by the relation

$$A_j(t) = ky_j \sum_{i=1}^I P_i \left\{ 1 - \exp[-\lambda_j(t_i - t_{i-1})] \right\} \exp \lambda_j(t - t_i) \quad ,$$

where $A_j(t)$ = the activity of the nuclide j at time t in disintegrations per minute, P_i = the average reactor power during the interval from t_{i-1} to t_i , k = the fission rate of the sample per unit reactor power, y_j = the fission yield of nuclide j , λ_j = the decay constant of nuclide j , t_i = the time at the end of interval i . Values of k were calculated from uranium fuel loadings and radiochemical burnup data. The in-pile release fractions were obtained by dividing the amount found in the thimbles by the total amount of the isotope produced. The results are shown in Table 3.10 along with postirradiation annealing data on some of the same samples.

The particles from lot 3325-117E irradiated at 925°C showed good retention of metallic fission products, whereas those irradiated at 1250°C showed some failure. Thus these data indicate that irradiation at the higher temperature was more damaging to the particles. The high release of cesium

Table 3.10
FRACTIONAL RELEASES OF PARTICLES IRRADIATED IN CAPSULE P13H

Sample No.	Irrad Temp (°C)	Burnup (% FIMA)	Cs ¹³⁷		Ce ¹⁴⁴		Sr ⁸⁹		Eu ¹⁵⁵
			In-pile	Post-irrad. ^a	In-pile	Post-irrad. ^a	In-pile	Post-irrad. ^a	In-pile ^a
3325-111E	1225	22.6	1.3×10^{-1}	---	5.1×10^{-3}	---	1.6×10^{-1}	---	9.0×10^{-4}
3325-113E	1250	22.6	4.5×10^{-2}	8.7×10^{-2}	2.7×10^{-3}	5.1×10^{-4}	3.5×10^{-2}	3.0×10^{-2}	1.5×10^{-4}
	900	22.6	---	---	---	---	---	---	---
3325-123E	1270	17.9	1.2×10^{-2}	---	5.3×10^{-4}	---	1.2×10^{-2}	---	---
3325-125E	1250	23.2	2.5×10^{-3}	3.1×10^{-4}	2.4×10^{-3}	1.4×10^{-2}	1.4×10^{-2}	6.0×10^{-1b}	8.2×10^{-5}
3325-117E	1250	23.2	5.6×10^{-2}	3.4×10^{-3}	1.8×10^{-3}	8.0×10^{-3}	9.2×10^{-2}	6.7×10^{-2}	4.7×10^{-4}
	925	22.8	7.7×10^{-4}	6.5×10^{-5}	2.9×10^{-5}	2.2×10^{-5}	2.7×10^{-4}	4.2×10^{-5}	---
3325-121E	1250	8.6	5.1×10^{-4}	---	4.8×10^{-5}	---	4.4×10^{-4}	---	1.4×10^{-6}
	975	8.0	$< 2.0 \times 10^{-4}$	---	4.8×10^{-5}	---	1.6×10^{-4}	---	8.8×10^{-6}
3325-119E	1260	8.8	5.4×10^{-4}	---	1.2×10^{-5}	---	1.2×10^{-3}	---	3.1×10^{-6}
3325-87E ^c	1300	17.5	8.3×10^{-2}	1.1×10^{-1}	1.4×10^{-2}	4.7×10^{-2}	1.3×10^{-1}	7.6×10^{-2}	8.9×10^{-4}
3325-89E ^c	1290	6.1	7.9×10^{-3}	---	4.3×10^{-3}	---	8.7×10^{-3}	---	3.2×10^{-5}

^a Release at 1400°C in 100 hr.

^b Unreasonably high.

^c ZrC barrier coatings.

indicated that failure of both the SiC and PyC layers probably occurred on some of the particles from lots 3325-113E, 3325-117E, and 3325-111E.

The agreement between the in-pile and the postirradiation release data is good in view of the uncertainties involved in radiochemical determinations and burnup analyses.

In summary, it appears that most of the samples tested at 1250°C in the P13H capsule showed some coating failure during the irradiation. The high releases of both cesium and strontium for some samples are indicative of both PyC and SiC coating failure.

In-pile Release from P13J Particles. In-pile release measurements on particles irradiated in the P13J capsule were initiated this quarter. Unfortunately, the six graphite half-thimble samples from each cell of the capsule were mixed together in the hot cell during disassembly, and it is therefore not possible to assign unambiguously a given pair of thimbles to a specific sample of particles. However, by comparison of the results of radiochemical analyses of the thimbles and crucibles and postirradiation annealing experiments on the fuel particles, it is believed that reliable in-pile release values for each sample can be obtained.

Fission-product assays were conducted on each half thimble, and the thimbles were then paired according to the relative amounts of fission product found. The data processed to date are presented in Table 3.11.

Three TRISO-coated samples were irradiated in cell 1 at 1000°C; the burnup and total fissions were essentially the same for all particles. In-pile cesium, cerium, and barium fractional releases for only one of the batches was less than 10^{-3} , indicating that only one sample remained relatively intact at the end of the irradiation. The other two batches of particles showed higher fractional releases for Cs^{137} , Ba^{140} , Sr^{89} , and Ce^{144} than are expected from intact TRISO particles. Metallographic examination of the particles in cell 1 indicated that the particles from lot 3592-35E-2 had approximately 6% to 9% coating failure during irradiation. Lot 3592-37E-2 (1000°C irradiation) showed no coating failure on metallographic examination, but showed 0.7% failure in the 1300°C irradiation. The samples exhibiting the intermediate release were therefore assumed to be 3592-37E-2. The in-pile metallic-fission-product release probably resulted from the observed coating failures. However, the extremely high in-pile strontium release for the lot 3592-35E-2 particles tends to indicate a higher degree of SiC layer failure than indicated by metallographic examination. The 3592-35E-2 sample contained only a 15- μm SiC coating with a 10- μm outer PyC layer.

The particles in cell 2 were the same as those in cell 1, but the irradiation temperature was 300°C higher, being approximately 1300°C. Two batches of particles showed poor retentions ($>10^{-3}$ fractional release for metallic fission products), whereas the third sample showed low release fractions for Cs^{137} , Ba^{140} , Sr^{89} , and Ce^{144} . The highest release data were again attributed to lot 3592-35E-3 because of metallographic evidence of 1% to 4% coating failure at 1300°C. Lot 3592-37E-3 showed 0.7% coating failure and was assumed to be the sample with the intermediate release.

Table 3.11
PRELIMINARY IN-PILE RELEASE OF SAMPLES IRRADIATED IN THE
P13J CAPSULE

Cell No.	Irrad. Temp (°C)	Sample No.	Type of Coating	SiC Layer Thickness (μm)	Fractional Release			
					Cs ¹³⁷	Ba ¹⁴⁰	Ce ¹⁴⁴	Sr ^{89a}
1	1000	3592-35E-2	TRISO	15	1x10 ⁻²	6x10 ⁻³	2x10 ⁻⁴	2.0x10 ⁻¹
		3592-37E-2	TRISO	25	3x10 ⁻³	3x10 ⁻⁴	<10 ⁻³	1.2x10 ⁻³
		3592-45E-3	TRISO	37	6x10 ⁻⁴	<10 ⁻⁴	<10 ⁻³	5x10 ⁻⁵
2	1300	3592-35E-3	TRISO	15	6x10 ⁻²	7x10 ⁻²	4x10 ⁻²	3x10 ⁻¹
		3592-37E-3	TRISO	25	<10 ⁻³	3x10 ⁻³	<10 ⁻³	2x10 ⁻²
		3592-45E-3	TRISO	37	<10 ⁻³	9x10 ⁻⁶	<10 ⁻³	5x10 ⁻⁴
3	1300	3592-69E	TRISO	29	1x10 ^{-2b}	~10 ⁻⁴	~10 ⁻⁴	N.D.
		3592-71E	TRISO	35	1x10 ^{-2b}	~10 ⁻⁴	~10 ⁻⁴	N.D.
		3583-67E	BISO	--	(c)	(c)	(c)	N.D.
4	1300	3592-47E	TRISO	10	7x10 ⁻²	9x10 ⁻²	1x10 ⁻²	N.D.
		3592-53E	TRISO	10	6x10 ⁻²	5x10 ⁻²	4x10 ⁻³	N.D.
		3592-73E	BISO	--	(c)	(c)	(c)	N.D.
5	600	3592-35E-1	TRISO	15	<10 ⁻⁵	<10 ⁻⁵	<10 ⁻⁵	1x10 ⁻⁴
		3592-37E-1	TRISO	25	<10 ⁻⁵	<10 ⁻⁵	<10 ⁻⁵	1x10 ⁻⁴
		3592-45E-1	TRISO	37	<10 ⁻⁵	<10 ⁻⁵	<10 ⁻⁵	9x10 ⁻⁵

^aN.D. = Not determined.

^bThis relatively high value probably resulted from release of Cs¹³⁷ by the BISO-coated sample and transport of the Cs¹³⁷ to the thimbles holding the TRISO-coated samples.

^cThimble containing sample too radioactive to analyze at this time.

Two TRISO-coated particle samples and one BISO-coated particle sample were irradiated in cell 3 at about 1300°C. The SiC-coated particles showed good retention for Ba¹⁴⁰ and Ce¹⁴⁴ (approximately 10⁻⁴). The Cs¹³⁷ release rate was 1.0 x 10⁻², indicating that a large amount of Cs¹³⁷ contamination probably occurred by vapor transport from the highly releasing BISO-coated particle batch irradiated in this cell; thus, the graphite thimbles from the low releasing SiC-coated particles are cesium contaminated. The thimbles containing the BISO-coated particles were too radioactive to analyze at this time.

Two TRISO-coated particle samples and one BISO-coated sample were irradiated in cell 4 at 1300°C, and all three showed very poor retention of metallic fission products, as shown in Table 3.11.

The particle batches in cell 5 were the same as those in cells 1 and 2, but the irradiation temperature was only about 600°C. The metallic fission-product release fractions were below the limits of detection ($<5 \times 10^{-5}$ fraction) for Cs^{137} , Ba^{140} , and Ce^{144} . The strontium release was of the order of 10^{-4} fraction for the three samples irradiated in this cell.

Although the data are not complete, one correlation apparently can be made. The samples with thin SiC coatings ($\leq 25 \mu\text{m}$) showed significant releases of metallic fission products at the higher temperatures during irradiation. There is some evidence that part of the metallic fission-product release might be due to defective particles.

Fuel Migration

The results of experiments designed to study the stability of fuel particles at elevated temperatures were presented in the previous quarterly report (GA-8600). To complete this series of experiments, two anneals at about 3000°C using ThO_2 and $(\text{Th,U})\text{O}_2$ particles with BISO and TRISO coatings have been carried out. (One additional experiment using carbide kernels with TRISO coatings has also been completed.) The particles were fabricated into fuel rods and annealed for 10 hr under flowing argon in closed graphite (H-327) crucibles. Details of the samples, annealing conditions, and weight losses are given in Table 3.12.

Table 3.12
WEIGHT CHANGES AND ANNEALING CONDITIONS FOR SIMULATED
CORE HEATUP EXPERIMENTS

Run No.	Temp (°C)	Kernel Type	Coating Type	Weight Change of Crucible (%)	Weight Change of Compact (%)
4	3070	Carbide	TRISO	---	-13.95
5	3020	Oxide	BISO	-0.97	-11.47
6	3010	Oxide	TRISO	-1.40	-18.57

The physical appearance of the fuel rods and graphite crucibles after annealing was excellent, and the fuel rods were easily removed from the crucibles. However, metallographic and radiographic examination of the fuel rods before and after annealing showed a change in the physical structure of the particles. It appeared that the kernels of most of the particles had migrated completely through the PyC and SiC coatings, leaving a hollow shell, and that no material was present in the void originally occupied by the kernels. During metallographic preparation, these voids filled with potting compound. This behavior indicates somewhat more migration of oxide from the kernel than was observed with the carbide kernels. Radiographs show that the uranium and thorium contents of the coatings and graphite matrix material

are much the same as those found in previous anneals. Chemical analyses are currently being performed to determine the mass balances for the systems.

Evaluation of the concentration profiles of uranium and thorium through the graphite crucible for all six runs is nearing completion. The crucibles from the earlier experiments, which used carbide cores with BISO coatings, were sectioned at three places along the crucible wall and on the bottom to determine whether or not a concentration gradient arose due to gravitational forces on the molten carbides in the fuel rod. Initial evaluation of the data indicates little or no gravitational flow.

The diffusion profiles show a steep portion followed by a long tail, indicating that fast and slow components of diffusion may be involved in the transport of uranium and thorium through the graphite crucible. This behavior is similar to that being observed for strontium in reactor graphites at about 1000°C. The steep portion of the profile (slow diffusion rate) is believed to be due to bulk diffusion. An average diffusion coefficient of about 2×10^{-9} cm²/sec for uranium and about 3×10^{-9} cm²/sec for thorium was obtained from the steep portion of the profiles. (It should be noted that the Th/U ratio is approximately 10 and that these diffusion coefficients may be concentration dependent.) This is within an order of magnitude of the extrapolated data of Loch, *et al.*, (Ref. 12), who studied the diffusion of uranium in graphite over the temperature range 1800° to 2400°C. The fast-diffusion process, possibly vapor transport, is governed by an average diffusion coefficient of about 10^{-6} cm²/sec for uranium and thorium. Some difficulty was encountered in interpretation of the data for the faster mechanism, and the data are presently being reevaluated.

Weight changes for run 4 on TRISO-coated carbide particles are also given in Table 3.12. The compact was fabricated from fissile particles from lot 3332-105D (UC₂) and fertile particles from lot HTK-4657 (ThC₂) using a fertile to fissile ratio of 10:1. The annealing conditions are given in Table 3.12. Evaluation of the diffusion profiles obtained by sectioning the graphite crucible is now in progress.

Sorption and Diffusion of Fission Products

Sorption of Metallic Fission Products on Matrix Materials. Cesium and strontium sorption measurements on carbon matrix materials have been resumed. Sorption data are being obtained for matrix (binder and filler) materials used to bond the fuel particles together and to act as a sorbent for metallic fission products. Relative sorption ratios (matrix:graphite) are needed to predict the release of these fission products from the core.

Cesium sorption measurements have been completed on D-16 and D-25 carbon matrix materials using the pseudoisopiestic method. The composition of these materials is given in Table 3.13.

Isotherms measured at 800°, 900°, and 1000°C show that these materials are virtually identical in their cesium sorption characteristics (see Fig. 3.11), even though charcoal was omitted from the D-25 mixture. This suggests that the binder residue carbon has a slightly higher sorption capacity than does the BC-589 charcoal.

Table 3.13
MATRIX MATERIAL COMPOSITIONS

Component	Composition (wt-%)	
	D-16	D-25
R-72S phenolic resin	43.8	49.2
Maleic anhydride	14.6	16.4
Nadic methyl anhydride	14.6	16.4
GP-38 graphite flour	18.0	18.0
BC-589 charcoal	9.0	--

Using these data and sorption data for H-327 graphite, the relative sorption ratios presented in Table 3.14 were calculated.

Table 3.14
RELATIVE CESIUM SORPTION RATIOS

Temp (°C)	Cesium Pressure (atm)	Sorption Ratio ^a
1000	1×10^{-5}	226
1000	1×10^{-8}	206
900	1×10^{-5}	230
900	1×10^{-8}	226

^a Matrix/graphite.

Similar measurements were made to obtain a sorption ratio for strontium using D-25 matrix material. A sorption ratio of 103 was measured at 1000°C with a strontium pressure of 2.3×10^{-8} atm. This value is believed to be low as a result of partial oxidation of the strontium metal supply during the run. A second run is in progress at 1000°C with a strontium pressure of about 2×10^{-7} atm to check the previous data.

Strontium-Graphite Dust Sorption Experiments. An experiment to measure the relative sorption of strontium on graphite dust and steel surfaces has been concluded after 135 days of operation. Graphite dust impregnated with strontium tagged with Sr^{85} was applied to five Incoloy 800 tube specimens and annealed in helium at 430° to 740°C. At the end of the run, the specimens were removed and radioassayed for strontium before and after removal of the dust by (1) high air velocity created by applying a vacuum and (2) wiping the surface with acetone.

Fig. 3.11--Sorption of Ce on D-16 and D-25 carbon matrix materials

The experimental apparatus, procedure, and results of a 5.6-day run were described in previous quarterly reports. Table 3.15 shows the results and a comparison of the 5.6-day and the recent 135-day runs.

Table 3.15
FRACTIONAL RETENTION OF STRONTIUM ON INCOLOY 800

Temp (°C)	Fraction of Strontium Remaining on Incoloy 800			
	5.6-day Run		135-day Run	
	After ^a Blowdown	After ^b Swab	After ^a Blowdown	After ^b Swab
900	0.9-1.0	0.7	---	---
800	0.9-1.0	0.9-1.0	---	---
750	---	---	0.9	0.7
700	0.9	0.7	0.9	0.7
650	---	---	0.8	0.6
600	0.3-0.7	0.2	0.8	0.6
500	0.25	0.1	0.6	0.4
450	---	---	0.5	0.3

^a Blowdown using a vacuum technique; estimated air velocity 55 ft/sec.

^b Swabbed with acetone-soaked tissue.

The fraction of strontium on the Incoloy 800 at 700°C was independent of the run time; however, at the lower temperatures the fraction of strontium transferred to the Incoloy 800 surface was greatly increased.

It is important to note that 60% to 80% of the strontium remaining after blowdown still remained after a thorough swab with acetone-saturated wipes.

The conclusion from these tests is that if heated metal surfaces are thinly coated with strontium-loaded graphite dust a large fraction of the strontium will transfer in a short time to the metal and will be held there quite tenaciously. It is believed that the mechanism for sorption of strontium on a metal surface is by dissolution into or association with the metal oxide film. The thermodynamics of such a system are difficult to calculate quantitatively, but it is undoubtedly true that the metal oxide-strontium system is much more stable than the Sr-C system at temperatures below 800°C.

HELIUM PURIFICATION SYSTEM

The objectives of this subtask are to:

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

Sorbent Beds

Low-temperature Adsorption Studies. The low-temperature adsorption screening tests have continued. The latest series of tests involved the measurement of krypton adsorption at room temperature using a helium purge. Other runs were performed at -196°C using a 25% CO - 75% helium mixture as the purge gas. The results of the tests are given in Table 3.16.

Table 3.16

MATERIALS SCREENING TESTS FOR THE LOW-TEMPERATURE TRAPPING SYSTEM

Material	Packing Density (g/cm ³)	Krypton Dynamic Adsorption Coefficient, K (cm ³ Kr/cm ³ adsorbent)			
		He Purge at Room Temp	25% CO - 75% Helium Purge at -196°C		
			1-g Bed	2-g Bed	10-g Bed
BC-502	0.63	69.0	8.2×10^3	2.5×10^4	3.8×10^4
GI-7883	0.40	24.9	1.9×10^4	---	1.9×10^4
GC-6/14X	0.36	23.9	2.5×10^4	2.6×10^4	2.0×10^4
MI-6736	0.45	--	2.0×10^4	---	2.7×10^4
BC-107, 6-10 mesh	0.55	43.7	3.4×10^3	---	9.9×10^3
BC-107, 10-14 (comparable to other materials)	0.49	--	---	---	1.8×10^4

These data indicate that BC-502 is by far the superior adsorbent. The final selection, however, must include considerations of strength, dusting characteristics, cost, and availability.

Hydrogen Gettering Studies. A series of nine dynamic hydrogen sorption tests has been performed at 370°C using titanium sponge bed and experimental conditions described in the previous quarterly report (GA-8600).

The present series of tests was performed to determine whether a titanium bed could be subjected to repeated adsorption and desorption cycles without significant loss of adsorptive capacity. It is also of interest to determine the lowest plant operating temperature at which successful and reasonably rapid regeneration can be accomplished to permit design of the vessel.

For these runs, regeneration was accomplished by purging the bed with purified helium at either 530° or 630°C. Regeneration was deemed complete when the effluent hydrogen concentration was observed to be less than 10 ppm by volume at the regeneration temperature. Lowering the bed temperature to the sorption temperature, 370°C, invariably resulted in an effluent H₂ concentration of less than 1 ppm prior to each test. Table 3.17 lists the conditions and results of the tests.

Table 3.17
DYNAMIC HYDROGEN SORPTION ON TITANIUM SPONGE^a

Run No.	Regeneration Data		Concentration of H ₂ (ppm) in Effluent after Passage at 370°C		
	Temp (°C)	Time (min)	1000 liters	5000 liters	10,000 liters
1	(b)	(b)	<1	4	16
2	530	9500	<1	25	~60
3	530	9800	500	500	500
3A	630	300	<1	~10	~200
4	630	3600	<1	10	22
5	630	6200	<1	35	60
6	630	5200	<1	30	23
7	630	4000	<1	35	50
8	530	8500	20	500	500

^a Conditions:

Bed weight = 60.2 g Ti.

Bed size = 0.58 in. in diameter by 10 in. long.

Inlet gas mix = 500 ppm H₂ in helium.

Sorption flow rate = 1.6 liters (STP)/min.

Sorption temp = 370°C (700°F).

Regeneration flow rate = 1.0 liter (STP)/min.

^b Fresh Ti sponge.

Run 1 (described in the previous quarterly report, GA-8600) ran successfully for a total of 16,000 liters of gas, after which the bed was regenerated at 530°C. In run 2, the adsorption efficiency showed a marked decrease, but remained within acceptable limits. The bed was regenerated again at 530°C, and shortly after run 3 was started, hydrogen breakthrough was observed. The bed was then subjected to a very short regeneration (300 min) at 630°C, which apparently was sufficient to restore the bed to near its original efficiency (see run 3A). Runs 4 through 7 were all performed after regeneration at 630°C with little or no loss of adsorption capacity. Run 8, however, again showed that regeneration at 530°C was insufficient to restore the sorptive capacity of the bed.

These data show that, once saturated with hydrogen, a titanium bed can be successfully and repeatedly regenerated at temperatures above 630°C. A regeneration temperature of 530°C, although high enough to remove hydrogen from the sponge, is apparently not high enough to reactivate the material and restore its sorptive capacity. The higher temperature may be necessary to activate the titanium catalytic surface, either by dissolution of surface contamination, or by increasing the number of active sites (or the available surface area) due to a crystallographic phase change.

Strength of Sorbent Materials. An investigation into the strength, or dusting characteristics, of sorbent materials under high-flow conditions has been initiated. The materials of interest are those intended for use in the helium purification system, namely, molecular sieves and activated carbon. This program is of interest because high-flow rates through the fixed sorber beds could occur during pressure transients, the most extreme of which would be a sudden plant depressurization.

The experimental procedure is to flow dry laboratory air through a 4-in.-ID by 10-ft-high column of the sorbent material of interest. Gas velocities up to 500 ft/min will be used. Dust generated in the packed column due to abrasion at the high gas velocity will be collected downstream in a full-flow millipore filter. Both downward (normal mode) and upward flow will be used. Measurements of bed levitation during upward flow will also be made.

Tests on activated carbon have just begun. For downward air flows up to 200 ft/min, the amount of dust generated appears to be negligible (i.e., less than 1 mg/hr).

Decontamination

A study is under way to select detergents for removing particulate contamination from stainless steel parts of the fuel handling machine and other equipment. The detergent must be compatible with the plant decontamination system and must be nonionic for disposal through the liquid-waste system.

Four nonionic surfactants, a predominately nonionic commercial detergent, and a conventional laundry detergent that has been used for decontamination were selected for testing. The latter two detergents were considered to be "standards" by which to judge the performance of the nonionic surfactants. The conventional laundry detergent contains substantial amounts of ionic builders (such as perhaps polyphosphates, sodium sulfate, etc.). The predominately nonionic detergent was included to determine whether a commercial formulation containing small amounts of additive (normally sodium

carboxymethyl cellulose) to retard redeposition of soil would be measurably superior to unblended nonionic surfactants under test conditions. All detergents tested are liquids, except the laundry detergent which is a powder.

Two series of tests were completed this quarter. The abilities of the detergents to remove graphite dust from stainless steel were compared in one series of tests at 23°C and in another at 95°C. In both cases the nonionic surfactants performed about as well as the two "standards," removing about 95% of the Sr^{85} used to tag the graphite dust. All were superior to demineralized water alone, though there was little difference at 95°C.

The most noticeable difference between the detergents and water alone was in the dispersion of the removed dust. With water alone the dust floated on the surface and climbed the walls of the beaker. As expected, because of their ability to depress the surface or interfacial tension, the detergents dispersed the dust to the point that it could not be seen. Such dispersion in solution can be as important as the deterative, or cleaning action, at the surface of the specimen.

The test method is based on a modified ASTM procedure. Stainless steel specimens having a surface area of about 33 cm² were made from 0.01-in.-thick shim stock. The specimens were (1) soaked in carbon tetrachloride for 5 min, (2) wiped with paper towels (3) rinsed with carbon tetrachloride, (4) soaked in methanol for 5 min, and (5) then soaked in demineralized water for 5 min. Specimens showing no water break after 5 sec were immersed in two beakers of acetone, air dried for 5 min, oven dried at 100°C for 10 min, and then stored in a desiccator until needed as test specimens. Just before a test, the specimens were removed from the desiccator and contaminated with Sr^{85} -tagged graphite. For the two tests completed this quarter, the contamination was accomplished by inserting the specimens, one at a time, in a plastic bag containing the dust. The bag was contained in a second bag to prevent dust release. Dust was then rubbed onto the surface of the specimen for 25 to 30 sec with 5 to 7 lb of force using the thumb and forefingers of a doubly-gloved hand. The method was surprisingly reproducible (see initial loadings listed in Table 3.18).

The graphite dust (1 to 10 μm) was tagged with Sr^{85} . The size distribution for particles with diameters greater than 0.15 μm was determined from photomicrographs to be as follows:

<u>Particle Diam (μm)</u>	<u>Percent by Count</u>
0.30	5
0.75	15
1.50	16
2.25	16
3.00	32
4.50	12
>6.00	5

Table 3.18
RESULTS OF DECONTAMINATION DETERGENT TESTS

Detergent	Type	Conc. (wt-%)	Temp (°C)	Initial Loading (μg dust/cm ²)	Amount Removed by Test Sol. (%)	Amount Removed by Test Sol. and Rinse (%)	Final Loading (μg dust/cm ²)
Test No. 1:							
A	Nonionic	1.0	23	8.4	77 ^a	94	0.48
B	Nonionic	1.0	23	14	77 ^a	96	0.62
C	Nonionic	1.0	23	8.8	61 ^a	94	0.55
D	Nonionic	1.0	23	13	84 ^a	95	0.60
E	Conventional laundry	0.72 ^b	23	7.8	79 ^a	94	0.46
F	Predominately nonionic	10 vol-%	23	7.6	81 ^a	94	0.45
G Demineralized H ₂ O	Control	---	23	8.5	55 ^a	73	2.3
Test No. 2:							
A	Nonionic	1.0	96±3	17	94	97	0.44
B	Nonionic	1.0	96±2	12	92	96	0.43
C	Nonionic	1.0	97±1	14	94	97	0.35
D	Nonionic	1.0	95±3	18	84	96	0.74
E	Conventional laundry	0.77 ^b	95±3	22	97	98	0.52
F	Predominately nonionic	10 vol-%	95±2	22	94	97	0.70
G Demineralized H ₂ O	Control	---	94±3	16	93	95	0.85

^a These values could be up to 10% higher because of possible errors in this set of measurements.

^b A powder measured by volume, then weighed; equivalent to 3/8 cup/gal H₂O.

Detergent solutions were prepared with demineralized water (800 ml in 1000 ml beakers). Contaminated specimens were successively attached to a stirring motor, immersed in the respective test solutions, rotated at approximately 120 rpm for 5 min, and removed without rinsing. Specimens were gamma-counted before and after the test. They were then rinsed with a soft stream of demineralized water, twice on each side, using a total of about 65 ml of water, and recounted. As mentioned above, two series of experiments were run, one at ambient temperature, 23°C, and one at 95°C. The results are reported in Table 3.18, and test conditions are given in Table 3.19.

On the day following each of the two test series, the test solutions containing dispersed dust were filtered through 0.45 μ m membrane filters to determine the fraction of Sr^{85} in solution. The results are reported in Table 3.19.

Table 3.19
TEST CONDITIONS AND ADDITIONAL MEASUREMENTS

	Test 1	Test 2
Time between specimen precleaning and test, days	5	7
Duration of individual tests, min	5	5
Speed of rotation, rpm	114 $\pm$ 15	126 $\pm$ 12
Fraction of Sr^{85} activity on dust one day after test, % ^a :		
Detergents	30 $\pm$ 6	34 $\pm$ 9
Water alone (G)	11	35
Fraction of Sr^{85} activity in solution one day after test, %:		
Detergents	46 $\pm$ 8	59 $\pm$ 8
Water alone	44	58

^aPercent of amount originally present on specimen.

Relative to the detergents, water alone performed better at 95° than at 23°C. This was not due to the increased solubility of Sr^{85} resulting in smaller differences in total activity removed, but to increased dust removal as shown by the filtration data in Table 3.19. The percentage of activity on filter G (water alone) was about the same as the average for the detergent filters at 95°C, but appreciably lower at 23°C. Higher dust removal by water at 95°C might have been due to the reduced surface tension of water at higher temperatures, occasional bubbles of steam rising from the bottom of the beaker, immersion of a cold specimen into hot water, or perhaps simply statistical variations since only two experiments were run.

A number of other observations were made regarding the behavior of individual detergents. Most important was that detergent D seemed to break down at 95°C. After the test at 95°C, specimen D was covered with an oily film containing some graphite. The oily film was not easy to rinse off; 95 ml of water rather than 65 ml was used to rinse this specimen.

Results of the tests are encouraging. Nonionic surfactants performed as well as two commercial detergents containing ionic additives.

The stainless steel specimens used in these experiments were quite smooth (the roughness is being determined). Surface roughness is a factor in the retention of contaminants, and cleaning efficiency declines with increasing roughness. A definite incentive exists for providing smooth, polished (electropolished, plated, or finely machined) surfaces on equipment likely to require decontamination.

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Task IV

FUEL ELEMENT PROOF TESTING

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

The Peach Bottom reactor was started up on May 6, 1968, with the first proof test element in the All-11 core position. The primary circuit gaseous activity was observed to be significantly higher than predicted based upon the measured particle release data. The plant was shut down on May 11, and the element was removed from the reactor on May 23. When the reactor was returned to power operation on May 31, a decrease in circuit activity was observed at low power indicating that the proof test element may have been a contributor to the observed reactor circuit activity.

Fuel particle manufacture for the second proof test element is in progress, and the particles are being subjected to detailed evaluations.

The matrix material formulation selected for the fuel rods of the second proof test element is D-38 (21 wt-% GP-38 filler). A total of 62 rods, 1.00, 5.93, and 11.53 in. in length, will be fabricated in laboratory equipment, and 44 rods 14.31 in. long will be fabricated in production equipment. The simultaneous-metering particle loading system and injection-molding rod fabrication technique will be used in both cases. Almost all of the rods will be carbonized by heating to 1000°C in a nitrogen purge, and a few will be heated to 1000°C in a vacuum. The rods will be carbonized in place in the test element graphite fuel sections.

Piggyback graphite specimens for the second proof test element have been prepared from grades H-327, TS-688, TS-814, 9567 (Nuclear II), and RC4.

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.

DESIGN AND FABRICATION

Fabrication and assembly of the grapple head was completed during the quarter and preliminary adjustments and continuity checks were made. Fabrication of the remaining portion of the fuel transfer machine is continuing.

The detail and assembly drawings of the telescoping tubes and upper housing have been modified to provide for various postulated tube failures. Included in these modifications were a tube separation switch, load cells in the outer tube wire rope cables, and a safety snubber system to prevent excessive impact loads between tubes.

Additional modifications to the telescoping tube drawings are being made to simplify the design, to facilitate the final machining requirements, and to eliminate all welded joints. The post-weld stress relief required for the 410 stainless steel material would have caused excessive distortion and was therefore eliminated.

A computer program was written to determine fuel handling machine coordinates for fuel and reflector element locations in the reactor core, fuel handling machine storage rack, and fuel storage wells. Also, the output of this code gives the element handling sequence from a minimum refueling time standpoint.

CONTROL SYSTEM

Considerable effort was applied to the design of the fuel handling machine control hardware during the quarter. Fabrication is under way on both analog and digital circuitry. A majority of the system components have either been received or are to be received in the near future. Effort was also expended in defining machine service connections between the machine and the control console. Several drawings have been released to show cable routing and power distribution requirements.

The computer interface is complete and has been successfully tested. The computer output, along with a digital-to-analog converter, has been used to control a breadboard analog positioning loop. Wiring of the digital logic rack is continuing; the remaining printed circuit cards for this rack have been released for fabrication. Design of the safety logic has progressed, with emphasis being applied to defining the quantity and nature of the inputs and the desired control function of the output.

Design and layout of the automatic mode control panel has been finalized, and a formal bid for fabrication is being obtained.

In the area of analog control, the control loop for the grapple head rotation has been breadboarded and tested with an electro-mechanical simulation of the pneumatic control. This loop is now scheduled to be tested with the actual grapple head and a pneumatic controller.

Computer programming is progressing, with major effort being concentrated on flow block diagrams. The basic refueling program has been broken into a number of subroutines, which are controlled by a supervisory program with operator and data tape inputs. The subroutine approach was chosen to facilitate checkout of the program, and to allow changes to be made with a minimum of effort.

The vertical drive servo system is being studied after receipt of component Laplace transform characteristics from the vendor. The study will confirm the theoretical stability and response of the system. A critical review of failure modes is in progress.

Plans are being made for a test of the DC torque motors. Design of the test facility is complete, and an autoclave has been ordered. Special brushes have been ordered for testing in high-purity helium.

The purge system has been revised to agree with the functional requirements of two service areas instead of three with added requirements for helium supplies for static pressurized seals and pneumatically activated mechanisms. A waste gas connection was also added. Design of the purge control system is nearing completion.

A helium detection device for purge system interlocks has been breadboarded.

TEST PROGRAM

Modifications to the test tower were completed except for installation of the 15-ton overhead crane. Procurement for the simulated fuel elements was initiated but a hold was placed on the order for the castings that simulate the upper clad reflector elements. Exact definition of the locking keys for these elements has been delayed pending the outcome of a revised seismic analysis. Manufacture of the dummy core grid plate was initiated.

The grapple head was placed in the test support fixture as shown in Fig. I.1. Interface wiring was completed along with a preliminary checkout of electrical components. The atuoclave was checked out to verify its heating and helium leak-tightness. Air and environmental testing was initiated, but no results have been obtained as yet.

Fig. I.1--Grapple head and test support fixture

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 MARK II DESIGN AND MANUFACTURE

A layout has been completed to determine all the modifications required to adapt the newly designed bearing cartridge to the existing prototype circulator mountings. Detail drawings of required adaptor pieces have been completed.

Fabrication of all Mark II prototype production hardware was initiated during the quarter. With the exception of fasteners, miscellaneous hardware, and specification control drawings, all major drawings on the bearing assembly housing have been released for fabrication.

The production drawings for the compressor and turbine blading were reviewed and released. All production drawings for the Mark II prototype unit have been released. The helium check valve drawings are detailed and are being checked.

COMPRESSOR BLADE STRAIN GAGING

A complete breadboard circuit including the oscillator, carrier amplifier, rotary transformer, rectifier, output amplifier, and vibrating strain gage was set up and tested. The system performance as measured in this limited test was satisfactory. It was noted that some additional filtering is desirable in the output circuit to further suppress the carrier frequency.

Additional breadboard testing is planned with a dynamic strain simulator. This will provide exact data on frequency and strain level response.

A hot test of the selected strain gage is to be made. This test will determine the hot gage factor and also possible installation problems.

PROTOTYPE CIRCULATOR TESTS

During this report period the prototype circulator configuration was revised and a second series of tests with air was initiated. The air tests include the aerodynamic performance evaluation of the 0.55 diffusion factor compressor blading and also the developmental testing of devices designed to improve the compressor stall characteristics.

The diffusion factor is a description of the aerodynamic loading on the blades. The blades previously tested were designed for a 0.50 diffusion factor and therefore were more lightly loaded aerodynamically.

The revised circulator configuration included alterations for these tests other than the new compressor blading. The disk catcher was removed by cutting, and a bolted flange was added to the diffuser shroud assembly. Removal of the disk catcher for these low-speed (3100 rpm maximum) tests permitted monitoring of the flow characteristics closer to both the leading and trailing edges of the compressor rotor blades. Additional flow probe access holes were fabricated into the test vessel, diffuser shroud, compressor housing, and stator housing.

A further modification was the addition of boundary layer energizers in the compressor inlet immediately downstream of the main diffuser support struts (see Fig. II.1). These devices are designed to delay boundary layer flow separation at the compressor rotor blade tips, thereby improving the compressor stall characteristics.

Following some preliminary tests of the 0.55 diffusion factor compressor blading, data acquisition began on May 20. Initial results for a speed of 2160 rpm (the aerodynamic equivalent of 9550 rpm in helium) indicate performance lower than predicted, with negligible improvement of compressor stall characteristics. Data were also obtained for speeds of 1720 and 2580 rpm. Since the special set of boundary layer energizers (for the purpose of improving stall characteristics) had been installed for these tests, it was decided to perform the shutoff head tests and then remove the boundary layer energizers to determine compressor performance without them. Testing will then continue with this configuration. This procedure will provide a direct comparison between the 0.55 diffusion factor blading performance and the previously tested 0.50 diffusion factor blades (see earlier quarterly report GA-8420) and also will permit further evaluation of the effectiveness of the boundary layer energizers.

A second stall control device, a suction ring, is being designed for installation along an inlet flow streamline near the compressor blade tip. This device is intended to prevent flow recirculation at the blade tip and also provide more flow to the blade tip under operating conditions that induce compressor stall. The testing of this ring should be accomplished during the latter phase of these current development tests.

PRIMARY COOLANT SHUTOFF VALVES

The results of the tests on the light-weight (nonactuated) check valve demonstrated that it is satisfactory for the primary coolant shutoff valves.

Fig. II.1--Compressor inlet showing boundary layer energizers

The only performance requirement not met by this valve is the allowable pressure drop. However, this deficiency occurs only during operation at 0% reactor power conditions and is not unique to the light-weight valve concept. The resultant decrease in core flow is small (approximately 6%) and is believed to be acceptable.

The air test also demonstrated that the airfoils included in the design to assist in holding the valve open do not improve the performance and they will therefore be deleted.

A preliminary layout of the PSC valve, based on the light-weight check valve which has been under test, was completed. Adaptation of a design of this valve for testing in the Valmont prototype facility has been completed.

Further testing of this valve with a spoiler installed has been completed, and the data are being reduced.

The design study of the actuated butterfly valve by the Stratos Western Division of Fairchild Hiller Corporation has been completed.

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.

Following the motor bearing failure discussed in the previous quarterly report (GA-8600), the environmental test program continued and accumulated a total of 573,357 rod jogging cycles. Three further motor bearing failures occurred during that time apart from which the performance of this mechanism appears to be very satisfactory.

The second motor bearing failure occurred after 365,486 jogs and was evidenced by seizure of the two pinion-end bearings. (The first failure had been of the brake-end bearing.) These bearings had made a total of 24×10^6 revolutions since the start of the test. No obvious cause of failure could be detected and it was therefore concluded the bearings had reached their life expectancy; i.e., the solid film had been depleted by wear to the extent that lubricant was no longer present in the bearing and metal-to-metal contact occurred.

In an attempt to increase life expectancy, larger bearings were incorporated. The fractural outer race principle was employed to increase the number of balls in each race. This incidentally made possible the use of a one-piece retainer; therefore, the 17-4PM material selection used previously was retained. A thicker film of solid lubricant was left on the bearings.

These bearings failed after only 4×10^6 revolutions due to the generation of an excessive amount of solid film debris resulting, apparently, from the heavier film deliberately left on the raceways.

The same type of race was next assembled with a full complement of tungsten carbide balls; that is, with no separator. Burnishing of the solid film was performed similarly to the first bearing set installed. These bearings performed 8.7×10^6 revolutions before failure, bringing the total jog cycles on the balance of the mechanism to 573,357. The condition of this last set of bearings was similar to the other failures; i.e., a complete absence of solid film and a bright "orange peel" surface on the balls and raceways.

Following the last failure, a reevaluation was made of the related thermal expansion conditions between the bearings and their mating parts. It was found that in certain cases all the radial play provided in a cold condition could possibly be lost due to differential thermal expansion.

These cases were not necessarily applicable to the bearings that had failed; however, the study did indicate a need for increased radial clearances.

A new bearing design has been initiated which provides a reservoir of solid film in the form of a sintered bronze retainer insert compacted with molybdenum disulphide.

A reassessment of the duty cycle for a typical regulating rod resulted in a significant reduction in the number of jog cycles necessary to establish the minimum confidence level. The new number being 500,000 means that the mechanism, with the exception of the shim motor, has satisfactorily completed the cycling phase of the environmental test program. There remain some 130 slow and 1000 fast scrams to be completed.

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.

STEAM-WATER FLOW STABILITY TEST PROGRAM

Preliminary tests on the boiling stability test loop were performed at the vendor's facility in order to examine the boiling circuit performance at the earliest possible time. Performance was studied with respect to:

1. Static sensitivity - a characteristic inherent in the performance of parallel boiling channels defined as an amplification of steady-state disturbances causing differences in mass flow rate between otherwise equal channels. In the PSC design, such disturbances encompass coil curvature, heating imbalances, and fabrication uncertainties. The magnitude of this sensitivity is required for use in predictions of performance imbalances between circuits.
2. Dynamic stability - the tendency of a once-through boiling channel to develop self-excited fluctuations in flow and temperature. Sufficient damping designed into the circuit can suppress these fluctuations to manageable levels. Since various boiling "noise" mechanisms are always present, it is not possible to completely eliminate all fluctuations.

PSC thermal-hydraulic operating conditions were simulated at full load, at one-quarter load, and at a depressurized case for a main steam coil circuit operating between subheaders. Pressure level and steam-water temperature distributions were duplicated, and local conditions were duplicated for the economizer-evaporator I locations with respect to mass velocity and heat flux. Since the test coil has a constant internal diameter in comparison to the varying internal diameter in the tubes, various exit orifices and inlet throttling adjustments were used on the test coil to duplicate the pressure drop of the PSC design, thereby simulating PSC hydraulic conditions. Induced throttling pressure losses in the test coil amounted to 60% of the total pressure drop. Exit orifices with nominal Δp values of 25 psi (Case A) and 60 psi (Case B) were used to simulate leadout tubing and the relative increase in hydraulic resistance in the superheat coils.

Data from a series of test runs at steady-state conditions are shown in Fig. IV.1. These data are plotted to describe the hydraulic characteristic (Δp versus flow rate) with corresponding exit steam temperatures adjacent to the plotted points. Sensitivity is exhibited in the gradient of the smoothed curves. The uppermost curve represents 100% load operation for the PSC plant in terms of duplicating the heat flux and mass velocity at an equivalent flow rate of 2.1 gpm in the economizer-evaporator I sections.

A summary listing of dynamic performance is shown in Table IV.1. This list indicates that fluctuations of inlet and exit flow rate at worst result in small variations in steam temperature. Thermal response to the full-load random flow fluctuations was completely attenuated. Quarter-load steam temperature fluctuations at the crossover did not exceed 5°F and these were fully attenuated upon reaching the superheater exit. The depressurized operating case clearly demonstrates the extreme thermal attenuation taking place in the presence of inlet water flow reversals.

Wall temperature response was not monitored in these preliminary tests. The loop has been delivered to the Gulf General Atomic site and is being modified to provide the additional information required to define the evaporator behavior. The planned testing program will then further examine stability and thermal performance.

REHEAT HEADER TEST

The steam side of a full-size steam generator reheat outlet collector and flow partition was tested with air at linear velocities duplicating the conditions of the PSC plant at full-load operation. This test was performed to determine (1) the degree of perforation of the partition necessary to reduce the reentry loss while satisfying the flow requirement to maintain dome cooling, (2) the effect of the partition upon the reheater tube-to-tube flow distribution, and (3) the clearance between the end cap and the end face of the partition that will provide for the minimum pressure loss. Results of the test are being analyzed.

TUBE JOINT DAMPING TEST

The tube restraint damping test will provide the final data necessary to interpret the GAVEL test data for the steam generator. Testing is performed on a rig as shown in Fig. IV.2 wherein the principal significant parameters, vibration amplitude and joint preload force and direction, are varied appropriately. Test data have been obtained on the PSC protective sleeve and wedge and will be obtained on a Sulzer wedge as used in GAVEL in the near future.

Wide data scatter was noted during the checkout runs. This was corrected by (1) placing the force gage next to the tube and the decoupling link next to the shaker, and (2) placing the support plates at the nodal points for free-free vibration mode.

Fig. IV.1--Preliminary testing for static sensitivity

Table IV.1
SUMMARY OF PRELIMINARY DYNAMIC STABILITY TESTS

PSC Module Simulated Operation	Inlet Pressure (psia)	Inlet Throttle Δp (psi)	Nominal Steam Temp Exit/Crossover (°F)	Inlet Flow Amplitude (%)	Oscillatory Period (sec)	Steam Temperature Amplitude (°F)	
						Crossover	Exit
100% load	2700	25	1050/750	(a)	---	~0	~0
25% load							
Case A	2400	2.2	1000/700	±20, max.	~15	~5	~0
Case B	2460	2.0	1100/800	±10, max.	~15	~5	~0
Depressurized	500	~1.0	1000/500	Flow rever- sal	~15	~30	~5

^a Periodic fluctuations were not detectable, random fluctuations were present.

Fig. IV.2--Tube joint damping test rig

The range of the magnification factor Q was found to be 24.7 to 110 for a preload force applied in-plane to the excitation direction. Although the range of magnification is quite broad, cross-plotting of Q against vibration amplitude and preload force indicates an expression of the general form:

$$Q = A^n[a + b(PL) + c(PL)^2] \quad ,$$

where a , b , c , and n are correlation constants, A is vibration amplitude, and PL is the preload force. The value of n is approximately -0.68, and a , b , and c are still to be determined.

The values for Q with the preload force applied perpendicular to the excitation direction were slightly lower than for the in-plane preload. This testing has been completed and the data are being analyzed.

Sulzer wedges as used in the GAVEL test are being tested.

GAVEL VIBRATION TEST

The GAVEL test is an air-flow simulation of the front half of the economizer-evaporator-superheater I bundle. The purpose of the test is to permit direct measurement of the flow-induced vibrations in a helical tube bundle.

Testing in Switzerland has been completed, and results of the no-flow portion of the Helmholtz testing have been received from Sulzer. Measured natural frequencies of various longitudinal waves agreed within $\pm 5\%$ of calculated values. Radial transverse waves were also computed for the approximate case in which the center cylinder of the annular flow chamber is removed. The actual configurations should give results slightly higher than the calculation, particularly for the transverse waves. Test data showed that Modes 1, 2, and 3 were of the radial type and were 9% to 16% higher than calculated. The fourth mode was a transverse wave and was 43% higher than calculated. Extrapolation to PSC helium conditions puts these frequencies at 1250 to 1500 cps which is above coincidence with coherent vortex shedding frequencies.

Discharge velocity profile measurements made with the bundle rotated 120° from the original position indicated that the profile also rotated. The source of the nonuniform profile is therefore attributed to the bundle, and not to the test facility.

Data analysis is being performed for the special tubes in the GAVEL bundle welded to the support plate, filled with water, and hand fitted (relatively loose) wedges.

TUBE RESTRAINT TESTING PROGRAM

The purposes of the tests were to evaluate tube damage resulting from the vibratory motion of the tube in relation to the support plate and to select the best method for protecting the tube from significant damage.

As noted in the previous quarterly report (GA-8600), a protective sleeve and wedge for the ferritic tubes was vibrated at a peak amplitude of 0.016 in. for 10^8 cycles without any significant damage to the tube. A similar test has just been completed for the protective sleeve design to be used in the superheater II and reheater tube bundles. An Incoloy tube with an Inconel X-750 protective sleeve and an Incoloy 800 wedge simulating the reheater and superheater tube restraint were vibrated at a peak-to-peak displacement of 0.016 in. for 10^8 cycles at 1350°F. No evidence of self-welding or fretting was noticed; however, the tube appeared to be slightly polished. Metallographic examination is being performed. From the test results obtained so far, it can be concluded that the protective sleeve and wedge design will prevent significant tube damage.

STRESS RUPTURE TESTS

Sanicro 31 is a nickel-iron-chromium alloy made by Sandvik Steel Works, Sandviken, Sweden. It is comparable in chemistry to other nickel-iron-chromium alloys and meets the requirements of SB-163 and other pertinent parts of Sections II and III of the ASME Code.

A considerable amount of stress-rupture data has been obtained by Sandvik, the University of Michigan, and Gulf General Atomic. The bulk of the tests made by Gulf General Atomic and the University of Michigan were initiated because recent test results obtained by Sandvik on the Sanicro 31 fell below the published data for a similar alloy upon which ASME Code Case 1325 was based. As there is no metallurgical difference between the two alloys, a study was made of the test procedure adopted by Sandvik, in which 12 specimens are strung together in series. Each time a specimen ruptures, the furnace is allowed to cool, the broken specimen is taken out, and the system is reloaded, resulting in certain specimens that have undergone numerous heating and cooling cycles. The normal stress rupture test procedure involves a single specimen which sees no temperature and loading cycles.

The results to date indicate that the Gulf General Atomic and University of Michigan data for Sanicro 31 give longer stress-to-rupture life than the Sandvik tests on the same material. Those from Gulf General Atomic and University of Michigan compare favorably with the published data, whereas the Sandvik results do not. Data are shown graphically in Fig. IV.3. Sandvik is conducting stress-rupture tests on single samples of Sanicro 31 tube material for comparison.

STEAM GENERATOR TUBE PLUGGING MACHINE

Drawings for the stationary housing and turret positioning system were issued for construction during this period. This equipment is used during both tube plugging operations and reheat pipe seal welding operations. Different sets of special purpose tools are placed on the turret for these operations.

Fig. IV.3--Creep strength of Sanicro 31

The construction drawings for the reheat pipe seal weld tools were completed during the quarter.

Preparation of construction layouts for the tube plugging tools was initiated. The layouts for the weld preparation tool and the turret stabilizing system have been completed and work is proceeding on the welding head. These tools represent the major portion of this task as few changes are anticipated in the preliminary designs of the remaining tube plugging tools.

The construction drawings for the tube plugging machine service equipment cart structure, support columns, hanger bracket assembly, etc., have been checked and reviewed.

The preparation of internal wiring diagrams and control console layouts has continued. In addition, preliminary layouts for service cable routing, storage reels, clamps, etc., were prepared and reviewed. A service request was issued to prepare schematic diagrams for the pneumatic systems in the tube plugging machine and to make preliminary layouts showing the physical arrangement of this equipment within the machine and on the service cart. When all of the service line and control system layouts have been completed, they will be reviewed to assure that they are compatible with each other and with the basic design of the service cart and the tube plugging machine.

Inquiry requisitions have been issued for the mass spectrometer and vacuum pump which are to be installed on the service equipment cart of the tube plugging machine.

Review comments received on the specification for the viewing system in the tube plugging machine are being incorporated.

Metallurgical examination of sample plugs welded with equipment previously used in developmental tube plugging tests has been completed. Of the five visually acceptable samples examined, one was found to contain a leak when tested with a mass spectrometer. Liquid penetrant testing revealed that the same sample was the only one with a surface defect. Radiographic examination revealed a small defect in the leaking plug weld and another small surface discontinuity in a second plug weld. Three of the samples were sectioned for macro examination which indicated that a greater depth of fusion should be provided. The suggested methods for attaining this objective are an increase in welding current and/or a decrease in welding speed. Current plans call for additional test welds to further refine the plug welding cycle.

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.

COATED PARTICLE EVALUATION

TRISO Particles for Capsules P20 and P21

Preparation of the coated particle samples for capsules P20 and P21 was completed. Each capsule contains twelve coated particle types in fifteen different locations. Both TRISO-I and -II designs, and LTI and HTI coatings, are included. The samples are described in Tables 3.8 and 3.9 in Part I of this report.

The coated particles used in these capsules were subjected to detailed characterization in order to aid in the postirradiation examination. A total of 48 lots of TRISO-coated particles were evaluated for irradiation testing in capsules P20 and P21, and 24 lots were selected for the capsules. The evaluation included chemical, visual, metallographic, and radiographic inspection of the coated particles.

One of the most common abnormalities noted during metallographic examination was a "deposit-layer" or "attack-zone," which was found associated with the SiC layer (see Figs. V.1 through V.4). Microprobe analysis of this zone showed that it was composed primarily of silicon and carbon, with no other element found in any reportable quantity. It should be noted, however, that there is always a distinct boundary line between the SiC and unknown material (Figs. V.1 and V.2). This tends to indicate that the materials are two completely different phases and possibly are compatible at a given temperature. An alternative explanation is that the unknown material is SiC that had been deposited under different conditions than the main SiC coating; however, this does not explain the apparent interaction zone shown in Fig. V.2. The unidentified material was also, in rare cases, found to occur in the form of a pipe (duct) which sometimes led through the buffer to the core (Figs. V.3 and V.4). The structure indicates that possibly a SiC duct is formed during the application of SiC either by a buildup of SiC on the inside surface of a cracked buffer coat, or as the result of HCl attack of the core with a subsequent deposition of a (Th,U,Si,C) complex on either a porous or cracked buffer (buffer cracked due to the exertion of gas pressure formed by HCl attack of the core).

Fig. V.1--Coated particle from 3923-128E (TRISO-I HTI, 20 μm SiC) showing deposit of unknown material on inside of SiC coating. Note sharp boundary between SiC coating and deposit-layer; also, there is no apparent interaction between the two materials even though particle was subjected to temperature of 1900°C (HTI coating temp.). Compare Fig. V.2 (250X)

Fig. V.2--Coated particle from 3923-97E (TRISO-I LTI, 10 μm SiC) showing heavy interaction of material deposited on inner surface of SiC coating. Note sharp boundaries between SiC and unknown material layers. Particle was subjected to maximum 1415°C (see Fig. V.1). Note grey material in core; this is apparently an oxide and the only case having such extensive contamination. This is believed to be unconnected with defective SiC layer since numerous similar SiC layers were observed in the absence of any apparent core contamination (250X)

Fig. V.3--Coated particle from 3923-63E (TRISO-I LTI, 20 μm SiC) showing segment of supposed "pipe" or "duct." See Fig. V.4 for more detailed explanation (750X)

Fig. V.4--Coated particle from 3923-97E (TRISO-I LTI, 10 μm SiC) showing supposed "duct" through SiC and buffer. Note that core material looks like graphite sphere with metallic stringers distributed throughout. Core was originally either graphite particle or the main metallic constituent has been leached out (200X)

Another defect in the SiC coatings is shown in Fig. V.5. This very irregular shape was seen in only a few samples; however, in sample 3923-131E it was the predominant defect, with about 4% of the coatings showing such irregularities.

Metallographic examination also revealed that there was considerable variation in the carbon content, as well as in the homogeneity of the cores. Specifically noted was the segregation of the dicarbide phase; Fig. V.6 shows this type of structure. At this time there is no identification of the phases present.

The observed types of defects were tabulated, and an attempt was made to correlate the defects with specific particle classes or parameters. From this comparison, a number of relationships were indicated.

One of the more significant results was that the particles that exhibited the anomalous deposit zone on the inside of the SiC layer were in every case TRISO-I particles. This defect was never observed with particles of the TRISO-II configuration. Whether or not this observation is due solely to metallographic preparation techniques is a point to be debated; i.e., with TRISO-I particles the relatively soft buffer is normally pulled away from the SiC surface, thus possibly revealing a structure that might be hidden with the TRISO-II particles due to the retention of the inner ISO during polishing. It seems more credible, however, that the TRISO-II particles do not have this defect, since in no case was a structure similar to that depicted in Fig. V.2 observed.

Several of the batches with LTI coatings exhibited a significant amount of chipping of the SiC at the inner surface, presumably as a result of polishing. This suggests the possibility of a stress being exerted on the SiC layer resulting from the outer LTI ISO, or parameters related to its deposition.

Concerning the outer ISO layers, most of the coatings looked good. The only irregularity noted was found by comparing the LTI coatings prepared in production equipment with those prepared in the laboratory. The two LTI coatings produced in production equipment had gross lamellations (onion-layered), whereas the laboratory-produced material had a homogeneous, somewhat porous-appearing structure.

With the outer isotropic coatings that were doped with silicon, it appeared that the silicon was present in rings. This structure was very apparent in the radiographs and somewhat less noticeable with metallography.

The cores that exhibited segregation were all from the same batch of cores (3201) and had a Th:U ratio of 2.25:1. This structure could conceivably be the result of "coring" during cooling from the molten state, or due to the presence of a miscibility gap in the liquid phase.

The results of this examination will be useful when the postirradiation examinations are evaluated, since some of the failures may be a direct result of production-type defects.

Fig. V.5--Coated particles from 3923-131E (TRISO-I HTI, 15 μm SiC) showing irregularly shaped SiC coating. A much higher failure rate would be expected with such particles than with particles having regularly shaped coatings (200X)

Fig. V.6--Particle from core batch 3201 (Th:U = 2.25:1) showing segregated dicarbide phase. Primary phase is assumed to be Th-rich $(\text{Th,U})\text{C}_2$ (grey), and matrix is most likely U-rich $(\text{U,Th})\text{C}_2$ phase (white) (900X)

Examination of the photomicrographs of the SiC coatings on the particles prepared for capsules P20 and P21 revealed that there are structure differences to be seen under polarized light. Additional observations must be made, but there is evidence that:

1. SiC coatings deposited at 1400°C (Table V.1) appear black or grey in photomicrographs under polarized light. Subsequent heat treatment (PyC overcoats) results in a trend to light grey and white as the overcoat temperature is increased. For an example, see Fig. V.7.
2. SiC coatings deposited at 1500°C (Table V.2) appear black under these conditions, and subsequent heat treatment results in no color changes (see Fig. V.8).
3. The color of the SiC coating on the disk does not necessarily agree with that on the particle.
4. There may be a correlation with thickness of coating (batch 4032-131, 5 μ m, appeared white, although deposited at 1400°C).
5. There may be a correlation with operator. Table V.2 lists six batches of SiC deposited at 1500°C, as well as the 16 overcoated batches made from these six. The first three were applied by one operator, and the next three by another operator. The appearances of the coatings follow the same division.

These observations will be extended and will be factored into the other observations of SiC structure that are being made.

TRISO Particles for Capsule P22

Preparation of TRISO particle samples for capsule P22 has been completed. Eighteen samples will be irradiated to doses of 8×10^{21} n/cm² ($E > 0.18$ Mev) and 20% FIMA burnup at temperatures of 900°, 1100°, and 1300°C. All of the samples have an SiC barrier coating. Eleven of the eighteen samples were prepared in production equipment. The remaining seven, including two fertile samples, were prepared in laboratory equipment. The final particle selection for capsule P22 is shown in Table V.3. The samples for P22 may be grouped into six general categories:

1. Variable Density Series. The particles have overcoating densities ranging from approximately 1.85 to 1.95 g/cm³ and are otherwise identical within a particle class (i.e., TRISO-I, TRISO-II). The experimental objectives are to obtain additional irradiation data to aid in selecting the optimum overcoat density.
2. Thick SiC Layer Series. Two particle batches are included to evaluate the irradiation behavior of thick (25- μ m) SiC coatings as a backup in case 20- μ m coatings prove inadequate.
3. Thin SiC Layer Series. Two particle batches with thin (15- μ m) SiC coatings are being included to aid in establishing minimum SiC thicknesses so neutron penalties can be reduced.
4. Fertile Particle Series. Two fertile particle batches are included to obtain irradiation performance data on SiC-barrier-coated fertile particles.

Table V.1

SILICON CARBIDE COATING BATCHES AND OVERCOAT BATCHES DEPOSITED
(SiC Deposited at 1400°C)

SiC Batch No.	SiC Thickness (μm)	Appearance		Overcoat Batch No.	Outer PyC Deposition Temp ($^{\circ}\text{C}$)	Used in P20	Used in P21
		Disk	Particle				
4032-121	14	Black	Grey	3923-49	1415		
			White	3923-51	1850		
			White	3923-135	1900		X
4032-123	18	Black	Grey	3923-57	1415		
			Grey	3923-83	1850		
			Grey	3923-113	1415		X
4032-125	18	Grey	Grey	3923-53	1415		
4032-127	20	Grey	Grey	3923-63	1415		X
			Grey	3923-69	1850		
			Grey	3923-71	1850		X
			White	3923-73	1900		
4032-129	9	Grey	Grey	3923-61	1415		
4032-131	5	White	Grey	4032-131	2000		
4032-133	20	Grey	Grey	3923-85	1415	X	
			White	3923-79	1850		
			White	3923-127	1900	X	
			White	3923-115	1850		
4032-135	10	Grey	Grey	3923-97	1415	X	
			White	3923-137	1900	X	
4032-139	20	Grey	Grey	3923-81	1850		
			Grey	3923-87	1415	X	
4032-151	15	Grey	Grey	3923-101	1415	X	
			Grey	4042-7	1350	X	
			White	3923-141	1900	X

3923-85E; 1415°C

3923-79E; 1850°C

3923-127E; 1900°C

Fig. V.7--Batches of material overcoated with PyC at three different temperatures. Same lot of starting material including the SiC layer (4032-133E) used for all three batches. Note that appearance of SiC layer depends on temperature of overcoating, growing whiter as the temperature is increased. SiC deposited at 1400°C. (Polarized light photomicrographs at 200X)

Table V.2

SILICON CARBIDE COATING BATCHES AND OVERCOAT BATCHES DEPOSITED
(SiC Deposited at 1500°C)

SiC Batch No.	SiC Thickness (μm)	Appearance		Overcoat Batch No.	Outer PyC Deposition Temp ($^{\circ}\text{C}$)	Used in P20	Used in P21
		Disk	Particle				
4032-143	18	Grey	Black	4042-5	1350		
			Black	3923-103	1415		X
			Black	3923-111	2000		X
4032-145	14	Black	Black	4042-3	1350		X
			Black	3923-95	1415		X
			Black	3923-93	1850		
4032-147	15	Black	Black	3923-131	1900	X	
			Black	3923-99	1415	X	
			Black	4032-149	1350	X	
			Black	3923-151	1900		
4042-21 ^a	20	Grey	Grey	3923-139	1900		X
			Grey	3923-147	1900		
			Grey	4023-77	1950		
4042-23 ^a	21	Grey	Grey	3923-145	1900		
			Grey	3923-149	1900	X	
4042-29 ^a	14	Grey	White	4042-31	1900		

^a Inclusions in coating; needs additional interpretation.

3923-103E; 1415°C

3923-111E; 2000°C

Fig. V.8--Two batches of materials overcoated with PyC at two different temperatures. Same starting material including the SiC layer (4032-143E) used for both batches. Note that appearance of SiC layer is jet black at left and only slightly lighter in photomicrograph at right. Color change, despite higher temperature of 2000°C, is not nearly as extreme as that in Fig. V.7. SiC deposited at 1500°C. (Polarized light photomicrographs at 200X)

Table V.3

TRISO PARTICLE SAMPLES TO BE IRRADIATED IN CAPSULE P22
 [DESIGN BURNUPS 20% FIMA (FISSILE PARTICLES) AND
 FAST-FLUX EXPOSURES OF 8×10^{21} n/cm² (E > 0.18 Mev)]

Cell No.	Design Irrad. Temp (°C)	Particle Batch No.	Prepared ^a	Sample Description
5 (Top)	1100	ET-49(B)SL	Prod	TRISO-II-LTI variable density series
		ET-50(A)SL	Prod	TRISO-II-LTI variable density series
		ET-49(D)SL	Prod	TRISO-II-LTI variable density series
		ET-49(C)SH	Prod	TRISO-II-HTI variable density series
		ET-49(C)SH-2	Prod	TRISO-II-HTI variable density series
		3923-63E	Lab	TRISO-I-LTI variable density series
4	1300	ET-47(A)SL	Prod	TRISO-I-LTI variable density series
		ET-47(C)SL	Prod	TRISO-I-LTI variable density series
		4033-87E	Lab	TRISO-I-HTI reference
		ET-48(A)SH	Prod	TRISO-II-HTI thin SiC series
		4033-75E	Lab	TRISO-II-HTI special buffered SiC
		4042-95E	Lab	TRISO-II-HTI SiC-doped HTI
3	1300	ET-50(A)SL	Prod	TRISO-II-LTI variable density series
		ET-49(B)SL	Prod	TRISO-II-LTI variable density series
		ET-49(D)SL	Prod	TRISO-II-LTI variable density series
		ET-49(C)SH	Prod	TRISO-II-HTI variable density series
		ET-49(C)SH-2	Prod	TRISO-II-HTI variable density series
		3923-63E	Lab	TRISO-I-LTI variable density series
2	1300	ET-60(C)SL ^b	Prod	TRISO-II-LTI thick SiC series
		4042-129 ^b	Lab	TRISO-II-LTI fertile series
		ET-60(A)SH ^c	Prod	TRISO-II-HTI thick SiC series
		4042-105 ^c	Lab	TRISO-II-HTI fertile series
		4033-31E	Lab	TRISO-II-HTI reference
		ET-48(B)SL	Prod	TRISO-II-LTI thin SiC series
1 (Bottom)	900	ET-50(A)SL	Prod	TRISO-II-LTI variable density series
		ET-49(B)SL	Prod	TRISO-II-LTI variable density series
		ET-49(D)SL	Prod	TRISO-II-LTI variable density series
		ET-49(C)SH	Prod	TRISO-II-HTI variable density series
		ET-49(C)SH-2	Prod	TRISO-II-HTI variable density series
		3923-63E	Lab	TRISO-I-LTI variable density series

^a Prod means coated in production equipment; lab means coated in laboratory equipment.

^b ET-60(C)SL and 4042-129 blended together in the same crucible.

^c ET-60(A)SH and 4042-105 blended together in the same crucible.

5. Special Buffer SiC Particles. This batch is being irradiated to evaluate a possible particle coating configuration in which low-density PyC can be used for the final overcoating.
6. SiC-doped HTI Sample. This sample is included to evaluate the irradiation behavior of HTI coatings that have been doped with SiC during vapor deposition.

As shown in Table V.3, six of the 18 samples are to be exposed at three different temperatures in the capsule, giving a total of 30 samples to be irradiated.

Twenty-two lots of TRISO-coated particles were evaluated for irradiation testing in capsule P22, and 18 lots were finally selected for the experiment. The evaluation included visual, chemical, metallographic, and radiographic inspections of the coated particles. Out of 18 samples prepared in production equipment, only two were found to have rejectable SiC layers. There were no rejectable SiC coatings in laboratory-prepared samples, although one laminated coating (fertile particle) was included in the capsule in order to evaluate this structure under irradiation. As part of the preparation and evaluation procedure, particles that had been coated without a SiC layer were removed from the samples. A detailed metallographic analysis of the selected samples is in progress.

Silicon Carbide Coating Evaluation

Detailed metallographic examinations over a wide range of magnifications were conducted on coated particles being considered for irradiation experiments. The purpose of these examinations, conducted using conventional metallographic techniques, including polarized light, was to determine whether the silicon carbide barrier coatings were free from deposition defects that could result in inferior irradiation performance. These defects, when found in SiC coatings, usually fall into one or more of the following classes:

1. Laminations. This defect is usually visible only under polarized light and is probably caused by temperature fluctuations in the particle bed (or particles) during coating. Severe laminations generally result in low bend strengths at room temperature. In some cases it appears that delamination has occurred along the laminations during the application of high-temperature isotropic overcoatings. A badly laminated silicon carbide coating is shown in Fig. V.9.
2. Delaminations. This defect, which is characterized by actual separation of the silicon carbide, usually along laminations, becomes, in effect, a circumferential crack. This flaw is almost always associated with excessive laminations and is particularly common in SiC coatings deposited at 1400°C and overcoated with high-temperature isotropic carbon. A typical delamination is shown in Fig. V.10.
3. Random Porosity. As the name implies, this porosity is randomly distributed through the coating and may or may not be cause for rejection, depending on the severity. Random porosity to a limited

A. White light (750X)

B. Polarized light (750X)

C. Polarized light (150X)

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Fig. V.9--Badly laminated SiC coating in fertile particle. The laminations, not visible in A, are clearly revealed by polarized light, B. and C.. C also shows SiC coating to be somewhat granular. While this coating should exhibit reduced room-temperature bend strength, several structures of this type have survived irradiation exposures of approximately 2.2×10^{21} n/cm² ($E > 0.18$ Mev) and burnup of about 13% FIMA without failures

Fig. V.10--Fertile particle showing delamination of SiC barrier coating. Coating probably delaminated along a lamination during application of high-temperature isotropic carbon overcoat. This type of defect is rejectable (150X)

degree is probably present in all SiC coatings; however, if the porosity is excessive, the value of the coating as a fission-product barrier is questionable and, in addition, the coating would probably be weak. A silicon carbide coating that contains excessive random porosity is shown in Fig. V.11.

4. Continuous Porosity. This porosity is formed at some time in the coating process and is usually found distributed along laminations in the silicon carbide coating. This defect may or may not be visible under white light, even at high magnifications, but is quite easily seen using polarized-light techniques. It appears that the laminations described above are made up of networks of continuous pores. Figure V.12 shows photomicrographs of this type of defect.

For comparison with the defective silicon carbide layers illustrated in Figs. V.9 through V.12, photomicrographs of a good silicon carbide coating are shown in Fig. V.13. Scrutiny of these photomicrographs will show that the silicon carbide is completely free of any of the flaws described above.

The defective structures described probably result to some extent in reduced mechanical properties; this is being determined by testing specimens removed from coated disks. It is desirable, however, to establish whether or not these defects can result in early and/or catastrophic failure of the barrier coating during irradiation.

Preirradiation and postirradiation examinations were made of the SiC coatings on particles used in capsules P13F, P13H, and P13J (1) to see which types of defects appeared to result in the most failures, (2) to determine what was the most frequent defect in the SiC, and (3) to attempt to establish an accept-reject criteria for TRISO particles slated for irradiation experiments. The results of these examinations are summarized as follows:

1. No delaminated SiC coatings were found.
2. The remaining coating deposition defects were found in both failed and unfailed particle batches.
3. There did not seem to be any definite correlation for batches that had failures with any single defect type.
4. In some cases, laminations were present before irradiation and were not visible after irradiation.
5. In some SiC coatings, the degree of random porosity appeared to increase after irradiation.

These results suggest that the SiC barrier coated particles can have SiC coatings with somewhat severe defects and survive to the burnup and fast-flux exposures experienced in capsules P13F, P13H, and P13J.

An additional thermal cycling experiment was conducted to further aid in establishing acceptability limits for the SiC coatings. In this experiment samples of TRISO particles were loaded into a furnace that was evacuated and backfilled with helium before heating. The furnace and samples

A. White light (150X)

B. Polarized light (150X)

Fig. V.11--Excessive random porosity in SiC coating in a fertile particle batch. Porosity (visible in A) shows as bright spots under polarized light. Coating is rejectable because performance as a barrier coating is questionable

A. White light (200X)

B. Polarized light (200X)

Fig. V.12--SiC barrier coating having excessive continuous porosity. Porosity, barely visible in A is clearly shown in B under polarized light. Porosity appears distributed along deposition planes in the SiC and is probably an integral part of the laminations

A. White light (200X)

B. Polarized light (200X)

C. Polarized light (750X)

Fig. V.13--Appearance of good silicon carbide coating. SiC layer looks smooth on both inner and outer circumference and has very little porosity present, A. Examination under polarized light, B, shows uniform dark layer that is still evident in C at 750X. Note almost complete absence of porosity and of laminations.

were then heated to 2000°C in 5 min, held for 10 min, and cooled to about 600°C in 10 min. This cycle was repeated ten times before the particles were removed from the furnace. The samples, along with unexposed samples, were then subjected to metallographic analysis. The primary purpose of this experiment was to determine whether laminations present in the SiC coating would delaminate during these severe thermal cycles. In addition, four delaminated batches were included to observe the effects of the thermal cycling.

The test results are still being analyzed, but the following observations have been made:

1. Four fissile particle batches that contained laminations before thermal cycling were essentially unchanged with regard to the silicon carbide coatings after cycling to 2000°C.
2. Four fertile particle batches containing delaminations in the silicon carbide coatings delaminated further during thermal cycling. The optical activity of these SiC coatings increased drastically, changing from a dark grey color under polarized light to white after thermal cycling. The reason for this phenomenon is unexplained at this time.

The results of the preceding examinations and the thermal cycling experiments (although still incomplete) indicate that delaminated SiC coatings are undesirable. Additional data, both in-pile and out-of-pile, are needed to evaluate the other defects.

Thermal Stability

Long-term Tests of BISO-coated Particles. A long-term thermal stability test of BISO-coated particles has reached 8000 hr. These particles are at 1400°C under 2-psig helium. The next scheduled examination will be at 9000 hr. This test also contains one sample of HTI-BISO-coated particles that had previously been tested for 9300 hr at 1400°C in vacuum, so that this sample has now accumulated 17,300 hr of exposure.

Long-term Tests of TRISO-coated Particles. The test times given in Table V.4 of an earlier quarterly progress report (GA-8420) for the 1400°C tests of TRISO-coated particles were incorrect. The conclusions listed for the two TRISO batches (2107-31 and 1985-77) at 18,000 hr of exposure were for the examination at 8765 hr. The particles were subsequently examined after 18,000 hr, and the migrations were found to be the same as those observed after 8765 hr. The one difference noted is that there appears to have been migration of uranium through the pyrolytic carbon layer and a deposition, or buildup, of uranium on the inside surface of the silicon carbide layer. This appears in a radiograph as a thin bright ring of a heavy metal about 1 to 2 μm thick. This condition is noted on half of the particles in batch 2107-31, and on all the particles in batch 1985-77. Electron microprobe and metallographic techniques will be used to confirm this observation.

The six other TRISO batches at 1400°C actually were examined after 5000 hr instead of 6783 hr as indicated on the table. These particles are exposed to 1400°C and 2-psig helium, and have now been examined after 10,000 hr.

Table V.4

LONG-TERM THERMAL STABILITY TESTS OF SILICON AND ZIRCONIUM
CARBIDE BARRIER COATINGS AT 1400°C

Lot No.	Type of Barrier Coating	Time (hr)	Migration ^a (μm)
3325-87E	ZrC	0	0
		600	1
		1,200	1
		2,400	1
		5,000	1
		10,000	1
3325-89E	ZrC	0	8
		600	11
		1,200	20
		2,400	28
		5,000	35
		10,000	39
3325-117E	SiC	0	2
		600	2
		1,200	3
		2,400	2
		5,000	2
		10,000	4
3332-139E	SiC	0	2
		600	7
		1,200	6
		2,400	8
		5,000	12
		10,000	14
3332-141E ^b	SiC	0	0
		600	0
		1,200	0
		2,400	0
		5,000	0
		10,000	0
3332-149E	SiC	0	1
		600	1
		1,200	2
		2,400	2
		5,000	2
		10,000	1

^a Extent of movement of heavy metal from the core into the coating as revealed by microradiography.

^b The core on 3332-141E is thorium-uranium oxide; all others are thorium-uranium carbide.

The results of this examination are presented in Table V.4. The six batches have been returned to test, with the next examination scheduled after 15,000 hr.

Irradiated TRISO-coated Particles. In isolated instances, some interaction between TRISO coatings and cores have been observed in particles irradiated at 1300°C. Plans have been formulated for a program to determine the operating limits of temperature and time for TRISO-coated fuel particles. The chemical compatibility of the core, fission products, and SiC coating is under investigation in order to identify the reactants and the mechanism by which this reaction is brought about. Initial studies will include:

1. Microprobe examination of TRISO-coated specimens previously irradiated. Particles irradiated at different temperatures and showing varying degrees of reaction between core and coating will be examined to identify reactants.
2. High-temperature annealing of TRISO-coated specimens irradiated at low temperatures. This will be an attempt to duplicate structures observed after high-temperature irradiations in order to better delineate between irradiation and thermal effects.
3. Out-of-pile compatibility studies for SiC and TRISO coatings versus thorium, uranium carbide cores containing controlled additions of major fission products. This work will explore the contribution of fission products to the compatibility problem.

During this reporting period, the microprobe at Gulf General Atomic was shielded to allow analysis of radioactive samples, and it is now ready for use. Fission-product-doped thorium-uranium carbide materials are being prepared for compatibility studies.

IMPROVED TECHNIQUES FOR ESTIMATING ANISOTROPY IN PyC

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

The technique for determining the crystalline texture in a metal sheet by use of a diffractometer (Ref. 3) was investigated as a potential method for anisotropy measurements in PyC. In this method, a pole figure is obtained

by the use of both transmission and reflection techniques; a portion of the pole figure is determined by each technique. A narrow region of overlap permits normalization, so that a complete pole figure and the anisotropy can be determined. When the measurement is properly conducted, no absorption correction is necessary for data from reflected X-rays, but rather large corrections must be calculated and applied to the transmitted X-ray data.

Extension of this technique to determine anisotropy in PyC was of interest because of potential time savings over the present method. With the texture-of-sheet method, a complete coating disk (removed from the graphite substrate) could be used as a specimen without the need for slicing it into small, thin specimens. However, the greatest time saving potential was thought to be in the direct-reading of diffracted X-ray intensity by a counter, thus eliminating the need for photographic work.

Two disks of PyC coating (approximately 7 mm diam by 40 μ m thick) having BAF values of 1.07 and 1.09 were prepared and subjected to the texture-of-sheet method, which was used without alteration from that described in Ref. 3. Integrated diffracted X-ray intensity was measured at 10° intervals in one quadrant of the Debye ring by slowly moving the counter along the radius of the ring and recording the output. The relative X-ray intensity for each position was determined by subtracting the background and measuring the area under the curve produced by the counter output. The raw X-ray intensity data were plotted as a function of positions on the Debye ring. Absorption corrections were calculated for the transmitted X-rays and applied to the data. The calculated corrections were much too small to bring the data into agreement with the pole figure necessary to give the proper BAF. In addition, the reflected X-rays showed evidence of considerable absorption effects. The effort necessary to resolve these difficulties (due primarily to the low absorption coefficient of carbon) was not undertaken, because during the experiments it became evident that scanning the Debye ring with the counter was nearly as time-consuming as the photographic work that it replaced. The potential may exist for greater time savings with the automation that is feasible with this technique. However, the extensive analytical work and equipment modifications necessary to pursue the investigations do not seem warranted at this time.

ANALYTICAL STUDIES OF COATING DESIGN

Studies of TRISO-coated particle fuel integrity are being conducted using mathematical models. Schematic representations of spherical coated fuel particles are shown in Fig. V.14 for the TRISO-I and TRISO-II designs.

The mathematical model used in the TRISO-I studies is due to Kaae (Refs. 4,5). The Kaae model is an analysis of coating stresses induced by the buildup of fission gas pressure, anisotropic radiation-induced dimensional changes in the outer PyC layer, and differential thermal expansion of the SiC and PyC layers. The analysis also accounts for the influences of transient and steady-state radiation-induced creep in the PyC layer.

Fig. V.14--TRISO-I and TRISO-II coated fuel particles

Work on the TRISO-II model is presently nearing completion, and TRISO-II coating design calculations are now under way. The primary objective of these studies is to evaluate the influence of pertinent parameters (e.g., environmental, geometric, and physical and mechanical properties) on the mechanical integrity of the coated particles using the Kaae model computer program as an aid to the optimization of the coated particle design.

Revision of Input Parameters

Coating design calculations discussed in the previous quarterly report (GA-8600) were based on an initial and a standard set of input parameters. The standard set included an interrelationship between irradiation-induced dimensional change and fast-flux exposure and an irradiation-induced creep constant that were characteristic of high-temperature isotropic (HTI) PyC irradiated at about 900°C. During this reporting period, the standard set was extended to include dimensional change and creep parameters characteristic of HTI at 1200°C and above and low-temperature isotropic (LTI) PyC at 900°C and above. The effect of increasing the irradiation temperature of HTI from about 900° to 1200°C and above is to approximately double dimensional change (Ref. 6). The effect of using LTI at 900°C and above rather than HTI at 900°C is to double the dimensional change (Ref. 6) and halve the creep constant. LTI dimensional change is insensitive to irradiation temperature above 900°C. HTI dimensional change at 1200°C and above appears to be equal to LTI dimensional change (Ref. 7).

Correlation with Irradiation Test Results

The extended standard set of input parameters (see Table V.5) was used to calculate coating stress histories for coated particle samples irradiated in capsules P13F, P13H, and P13J, and for reference coated particles for various combinations of PyC type (LTI or HTI), PyC temperature, and PyC density. The maximum tensile and compressive stresses during the lifetime of the coated particles are shown in Table V.6 and in Figs. V.15 and V.16. The bars indicate the range in maximum tensile and compressive stresses corresponding to the corrected calculations of Table V.6. Calculated maximum stress is indicated separately when coated particle failure was observed. In addition, calculated maximum stresses in the reference fissile coated particle are indicated.

Changing PyC parameters from those corresponding to HTI at 900°C to the appropriate parameters in accordance with Table V.5 had the following effects. Doubling dimensional changes in HTI approximately doubled the maximum tensile stress in the PyC layer and doubled the maximum compressive stress in the SiC layer (with a few exceptions). The maximum tensile stress in the SiC layer was generally reduced by these changes, but good correlation between calculated stress levels and observed failures in capsules P13F and P13H was maintained.

The effects of doubling the dimensional changes and halving creep in LTI were to increase the maximum pyrocarbon tensile stress by a factor of about 4, and to increase the maximum SiC compressive stress by factors of from 5 to 68. The maximum SiC tensile stress was considerably reduced.

Table V.5

EXTENDED STANDARD KAAE MODEL INPUT PARAMETERS FOR TRISO-I COATED PARTICLES

Core diameter, μm	200 fissile, 400 fertile
Buffer thickness, μm	50 fissile, 50 fertile
SiC thickness, μm	20 fissile, 20 fertile
PyC thickness, μm	50 fissile, 60 fertile
Th in heavy metal, %	81 fissile, 100 fertile
U in heavy metal, %	19 fissile, 0 fertile
Theoretical buffer density, g/cm^3	2.2
Buffer density, g/cm^3	1.1
UC ₂ density, g/cm^3	11.3
ThC ₂ density, g/cm^3	8.6
Molar ratio of fission gas: fissions	0.5
SiC Poisson's ratio	0.33
SiC Young's modulus, psi	60×10^6
PyC Poisson's ratio	0.33
PyC Young's modulus, psi	4×10^6
Temperature of irradiation, $^{\circ}\text{C}$	1260
Temperature at which PyC (HTI) is deposited, $^{\circ}\text{C}$	1800
PyC coefficient of thermal expansion, $^{\circ}\text{C}^{-1}$	5.4×10^{-6}
SiC coefficient of thermal expansion, $^{\circ}\text{C}^{-1}$	5.5×10^{-6}
Transient neutron-induced creep constant	
in PyC, $(\text{psi-n}/\text{cm}^2)^{-1}$:	
HTI at $\sim 900^{\circ}\text{C}$	2.5×10^{-27}
HTI at $\geq 1200^{\circ}\text{C}$	2.5×10^{-27}
LTI at $\geq 900^{\circ}\text{C}$	2.5×10^{-27}
Steady-state neutron-induced creep constant	
in PyC, $(\text{psi-n}/\text{cm}^2)^{-1}$:	
HTI at $\sim 900^{\circ}\text{C}$	2.0×10^{-27}
HTI at $\geq 1200^{\circ}\text{C}$	2.0×10^{-27}
LTI at $\geq 900^{\circ}\text{C}$	1.0×10^{-27}
Neutron-induced dimensional changes	
in PyC:	
HTI at $\sim 900^{\circ}\text{C}$	Bokros curves, Case C (Ref. 8)
HTI at $\geq 1200^{\circ}\text{C}$	Twice Case C
LTI at $\geq 900^{\circ}\text{C}$	Twice Case C

NOTE: Burnup-fast-flux relationship: fissile, realistic curve (Ref. 8) from 0,0 to 20% FIMA, 8×10^{21} n/cm², where burnup rate decreases with neutron dose; fertile, realistic curve (Ref. 8) from 0,0 to 7% FIMA, 8×10^{21} n/cm², where burnup rate increases with neutron dose.

Table V.6
CALCULATED STRESSES IN TRISO-I COATINGS USING THE KAAE MODEL FOR CAPSULES
P13F, P13H, P13J, AND FOR REFERENCE CASES

Experiment	PyC Type	Irrad Temp ^a (°C)	Creep Constant (10 ²⁷ psi-n/cm ²) ⁻¹		Dim. Change Case ^b		Max. PyC Stress (10 ³ psi)		Min. SiC Stress (10 ³ psi)		Max. SiC Stress (10 ³ psi)		Failures (%)
			Original Calc.	Corrected Calc.	Original Calc.	Corrected Calc.	Original Calc.	Corrected Calc.	Original Calc.	Corrected Calc.	Original Calc.	Corrected Calc.	
P13F-1	HTI	1265	2	2	B-1	B-2	10.5	21.6	-9.8	-23.2	1.9	0.5	0
P13F-2	HTI	1250	2	2	B-1	B-2	10.2	20.6	-2.1	-6.3	8.5	4.6	0
P13F-3	HTI	1300	2	2	B-1	B-2	10.7	21.7	-1.5	-4.3	8.3	5.5	0
P13F-4	HTI	1290	2	2	B-1	B-2	10.8	21.6	-23.7	-52.9	0.8	1.0	0
P13F-5	HTI	1310	2	2	B-1	B-2	11.5	21.8	-2.2	-5.5	4.0	0.9	1
P13F-6	HTI	1305	2	2	B-1	B-2	13.1	24.1	-10.1	-22.3	0.6	0.4	0
P13F-7	HTI	1245	2	2	B-1	B-2	11.4	25.6	-10.5	-23.1	0.6	0.5	0
P13F-8	HTI	850	2	2	B-1	B-1	12.9	12.9	-11.1	-11.1	0.8	0.8	0
P13F-9	HTI	910	2	2	B-1	B-1	10.9	10.9	-1.8	-1.8	6.0	6.0	0
P13F-10	HTI	910	2	2	B-1	B-1	13.1	13.1	-25.5	-25.5	1.5	1.5	0
P13H-1	HTI	1225	2	2	A-1	A-2	30.0	60.5	-15.0	-51.2	0.5	0.6	26
P13H-2	HTI	1225	2	2	A-1	A-2	25.4	50.7	-2.9	-7.8	14.2	3.4	0.2
P13H-3	HTI	1260	2	2	A-1	A-2	19.6	39.3	-2.1	-6.4	8.6	1.6	0.2
P13H-4	HTI	1225	2	2	B-1	B-2	20.1	40.0	-4.6	-10.8	4.8	0.2	0
P13H-5	HTI	925	2	2	A-1	A-1	23.5	23.5	-2.9	-2.9	10.1	10.1	0.1
P13H-6	HTI	1000	2	2	B-1	B-1	17.8	17.8	-4.7	-4.7	3.4	3.4	0.2
P13J-1	LTI	1300	2	1	B-1	B-2	18.5	70.0	-3.1	-19.7	39.3	9.1	1
P13J-2	LTI	1300	2	1	B-1	B-2	18.4	69.9	-2.1	-12.9	23.5	4.7	0
P13J-3	LTI	1300	2	1	B-1	B-2	18.2	69.1	-1.0	-7.2	17.2	5.5	0
P13J-4	LTI	1300	2	1	B-1	B-2	19.7	74.8	-2.0	-135.3	23.0	0.1	0
P13J-5	LTI	1300	2	1	B-1	B-2	20.4	77.9	-31.0	-268.2	0.2	0.2	0
P13J-6	LTI	1300	2	1	B-1	B-2	18.3	69.6	-2.7	-12.6	9.9	0.2	0
P13J-7	LTI	1300	2	1	B-1	B-2	18.3	69.5	-2.3	-10.9	8.2	0.2	0
P13J-8	LTI	1000	2	1	B-1	B-2	18.5	70.0	-3.7	-21.8	29.4	0.2	6
P13J-9	LTI	1000	2	1	B-1	B-2	18.4	69.7	-2.5	-13.6	17.5	0.2	0.7
P13J-10	LTI	1000	2	1	B-1	B-2	18.2	69.1	-1.3	-7.7	13.0	2.4	0
P13J-11	LTI	600	2	1	B-1	B-2	18.4	69.9	-4.9	-22.5	16.2	0.3	0.2
P13J-12	LTI	600	2	1	B-1	B-2	18.4	69.8	-3.2	-14.6	9.4	0.2	0
P13J-13	LTI	600	2	1	B-1	B-2	18.2	69.1	-1.7	-8.4	7.4	0.1	0
Fissile ^c	HTI	900	-	2	---	A-1	---	35.3	---	-58.9	---	1.0	---
Fissile ^c	HTI	900	-	2	---	C-1	---	17.5	---	-32.3	---	1.0	---
Fissile ^c	HTI	1260	-	2	---	A-2	---	70.6	---	-126.6	---	0.6	---
Fissile ^c	HTI	1260	-	2	---	C-2	---	35.5	---	-74.6	---	0.6	---
Fertile ^d	HTI	900	-	2	---	A-1	---	33.2	---	-72.3	---	1.2	---
Fertile ^d	HTI	900	-	2	---	C-1	---	17.9	---	-44.2	---	1.2	---
Fertile ^d	HTI	1260	-	2	---	A-2	---	66.3	---	-150.1	---	0.7	---
Fertile ^d	HTI	1260	-	2	---	C-2	---	35.1	---	-95.5	---	0.7	---
Fissile ^c	LTI	1260	-	1	---	C-2	---	69.8	---	-163.8	---	0.2	---
Fertile ^d	LTI	1260	-	1	---	C-2	---	71.4	---	-201.6	---	0.2	---

^a Temperature remains constant in calculations; EOL temperature used for P-capsule calculations.

^b The original calculations were based on dimensional changes corresponding to HTI at 900°C and were reported in Ref. 8. The corrected calculations were done in accordance with the following conditions for the various cases:

A-1 and A-2 $\rho = 2.10 \text{ g/cm}^3$
 B-1 and B-2 $\rho = 2.05 \text{ g/cm}^3$
 C-1 and C-2 $\rho = 1.95 \text{ g/cm}^3$
 A-1, B-1, and C-1 HTI at 900°C
 A-2, B-2, and C-2 HTI at $\geq 1200^\circ\text{C}$ or LTI at $\geq 900^\circ\text{C}$

^c Reference fissile design.

^d Reference fertile design.

Fig. V.15--Range in coating stresses in samples from capsules P13F and P13H using extended standard input parameters with Kaae model. Sample failure is indicated for correlation, and maximum coating stresses in reference TRISO-I HTI fissile particle are shown for comparison. PyC is HTI in all cases. $K_{SS} = 2 \times 10^{-27} (\text{psi-n/cm}^2)^{-1}$

Fig. V.16--Range in coating stresses in 13 samples from capsule P13J using extended standard input parameters with Kaae model. Sample failure is indicated for correlation, and maximum coating stresses in reference TRISO-I LTI fissile particle are shown for comparison. PyC is LTI in all cases. $K_{SS} = 1 \times 10^{-27} (\text{psi-n/cm}^2)^{-1}$, $\Delta L = 2 \times \text{HTI}$

Correlation between calculated stress levels and observed failures in capsule P13J was good before the parameters were revised and poor after the revisions.

Stresses with Low-density Isotropic Coatings

Coating stresses were calculated for the TRISO-I design to evaluate the effects of using low-density outer PyC coatings. The calculations were performed using data from capsules GE-H-13-13, GE-H-13-14, and G12 (Ref. 9) on irradiation-induced dimensional changes in HTI with BAF less than 1.1, densities of 1.55 and 1.75 to 1.8 g/cm³, and irradiation temperatures of 1200°C. Using the reference fissile input parameters and the dimensional changes corresponding to the 1.75 to 1.8 g/cm³ HTI density, several runs were made for various HTI thicknesses. The use of low-density PyC of the reference 50-μm HTI thickness resulted in reduced coating stresses over those for the reference 1.95 g/cm³ HTI density. With 30 μm of the low-density HTI, the maximum PyC tensile stress was reduced to 21,900 psi and maximum SiC compressive stress was reduced to 27,700 psi. The SiC layer was kept in compression (except for thermal expansion stresses of 1000 to 2000 psi at beginning of life) when the PyC was 30 μm or above. The SiC stress reached 5700 psi in tension at end of life for an HTI thickness of 20 μm.

Attempts were made to allow for a buffer layer between the SiC and PyC layers to accommodate the rapid densification of an HTI with a 1.55 g/cm³ density. Figure V.17 shows the isotropic portion of the shrinkage and the resulting contraction of the radius of the inner surface of the HTI layer for each of the above HTI densities. Contraction of the HTI radius is expressed in microns and as a percentage of the interposed buffer for two buffer thicknesses. It is seen that to accommodate the isotropic portion of the HTI shrinkage, the interposed buffer must shrink and/or crush by 33% to 50% for an HTI density of 1.55 g/cm³ or by 27% to 38% for an HTI density of 1.75 to 1.80 g/cm³. The lower-density HTI undergoes more initial densification, but the rest of the dimensional changes are less severe than in the higher-density HTI. Since the Kaae model does not cover such a design, stress calculations were accomplished by adjusting PyC dimensional change to zero between doses of 0 to 0.5×10^{21} n/cm², at which point it was assumed that the hypothetical buffer layer began to transfer radial stresses to the SiC layer. Using reference fissile parameters and a 30-μm HTI thickness (which allows for a 20-μm buffer), maximum PyC tensile Stress and SiC compressive stress were improved over the reference coating, but PyC shrinkage was insufficient to maintain compression in the SiC toward the end of life. Maximum PyC tensile stress was 30,000 psi, maximum SiC compressive stress was 43,800 psi, and maximum SiC tensile stress was 14,500 psi.

This run was repeated using the lower creep coefficient corresponding to LTI coatings. (The irradiation-induced dimensional change was assumed to be the same as that for HTI, since the irradiation temperature was 1260°C in these computations.) The use of LTI was intended to maintain the SiC in compression. However, the SiC still went into tension toward the end of life. Maximum PyC tensile stress rose to 52,100 psi, maximum SiC compressive stress rose to 80,200 psi, and maximum SiC tensile stress was reduced to 7700 psi.

Fig. V.17--Initial dimensional change in HTI TRISO-I fissile particle with outer low-density ISO separated from SiC by a buffer layer

Effect of Size of Fast-flux Interval on Calculated Stresses

A computer run was made to establish the error in calculated stresses due to allowing the fast-flux exposure to increase in steps of 10^{20} n/cm² rather than in very small steps (or ultimately, a continuous increase). The calculations for the reference TRISO-I-LTI-coated fissile particle (1.95 g/cm³ LTI irradiated at 1260°C) were repeated using dose steps of 10^{19} n/cm². When coating stresses were changing rapidly with dose, the smaller dose steps resulted in 5% to 10% higher stresses; but when stress was changing very slowly with dose, the difference was very small. With the larger dose steps, the maximum SiC compressive stress was 1.6376×10^5 psi at 6.1×10^{21} n/cm² and maximum PyC tensile stress was 6.976×10^4 psi at 7.7×10^{21} n/cm². With the small steps, the maximum SiC compressive stress was 1.6407×10^5 psi at 5.91×10^{21} n/cm² and the maximum PyC tensile stress was 6.976×10^4 psi at 7.35×10^{21} n/cm².

Stress Calculations for ORNL B9-40 Irradiation Test

Coating stresses were calculated for the TRISO-I-LTI-coated UC₂ particles to be irradiated in the ORNL B9-40 experiment. The program was altered to provide for a change in temperature during irradiation, since plans call for increasing fuel temperature from 1300° to 1700°C at an exposure of 0.2×10^{21} n/cm². Although the temperature will be increased in 100°C increments, the program is only capable of accounting for a single change in temperature. It was assumed that burnup increased linearly to 15% FIMA at an exposure of 0.2×10^{21} n/cm² and then remained at 15% FIMA out to 0.25×10^{21} n/cm². These and other input parameters are summarized in Table V.7, and the results are shown in Fig. V.18. It may be noted in Fig. V.18 that the change in temperature (which causes an increase in fission gas pressure) causes a reduction in compressive stress in the SiC, but the stresses in the PyC are essentially uninfluenced. Compressive stress in the SiC reaches a maximum of 29,800 psi at the end of irradiation, and tensile stress in the PyC reaches a maximum of 15,700 psi, also at the end of irradiation. In comparison, the PSC reference TRISO-I-LTI-coated fissile particle, irradiated at 1260°C, reaches a maximum SiC compressive stress of 163,800 psi at 6.1×10^{21} n/cm² and a maximum PyC tensile stress of 69,800 psi at 6.9×10^{21} n/cm².

FISSION-PRODUCT RELEASE

The release of fission products from TRISO-coated fuel particles under reactor conditions arises from three sources:

1. The release of fission products born in the fission of uranium contamination in the outer pyrolytic carbon coating. For the metallic fission products strontium and barium, the steady-state fractional release from this source is assumed to be unity, since the pyrolytic carbon does not constitute a barrier to the metals.
2. The release of fission products born in defective particles. For the metallic fission products this includes not only those particles with broken or incomplete coatings, but also those with broken, thin, or totally absent silicon carbide layers. However, since the

Table V.7
KAAE MODEL INPUT PARAMETERS FOR THE TRISO-I-COATED
UC₂ PARTICLES USED IN ORNL CAPSULE B9-40

PyC dimensional change	Bokros curve corresponding to LTI at ≥900°C with a density of ~1.95 g/cm ³
Irradiation temperature	1300°C to a dose of 0.2x10 ²¹ n/cm ² ; 1700°C thereafter
LTI deposition temperature, °C	1250
UC ₂ core diameter, μm	212
Buffer thickness, μm	43
SiC thickness, μm	22
PyC thickness, μm	69
UC ₂ core density (assumed) g/cm ³	11.3
Molar ratio of fission gas: fissions	0.5
Poisson's ratio for SiC	0.33
Young's modulus for SiC, psi	6x10 ⁷
Poisson's ratio for PyC	0.33
Young's modulus for PyC, psi	4x10 ⁶
Thermal expansion coeff. for SiC, °C ⁻¹	5.5x10 ⁻⁶
Thermal expansion coeff. for PyC, °C ⁻¹	5.4x10 ⁻⁶
Transient creep coeff., rad.-induced, (psi-n/cm ²) ⁻¹	2.5x10 ⁻²⁷
Steady-state creep coeff., rad.-induced, (psi-n/cm ²) ⁻¹	1.0x10 ⁻²⁷
Buffer density, g/cm ³	0.7
EOL fast-flux exposure, n/cm ²	0.25x10 ²¹

NOTE: Burnup-fast-flux relationship: straight line from 0,0 to 15% FIMA,
0.2 x 10²¹ n/cm². Burnup is assumed to remain constant above 0.2 x 10²¹ n/cm².

Fig. V.18--TRISO coating stresses in UC₂-coated particles in the B9-40 experiment

kernel of such particles still retains the metallic elements to some extent, it is assumed that the steady-state fractional release from this source is about 0.5. This is probably a minimum value under steady-state conditions. (The effect of burnup on kernel degradation is not considered here.)

3. The intrinsic release of fission products from intact, high-quality particles. The steady-state fractional release from this source is thought to be very low, perhaps less than 10^{-7} .

Fission-product release studies using current procedures measure the total release from all three of the above sources, and it has not been possible to date to determine unambiguously the release from the individual sources. It is considered important to separate these sources of fission-product release for several reasons. First, if one source should prove to be predominant, efforts can be made to reduce its contribution. Second, accurate calculations of the release from the reactor core cannot be made unless the fractional release due to each source and the relative contribution of the source are both known.

The necessity of controlling both contamination and defective particles is shown in Table V.8, where the fractional releases due to each of these sources is given as a function of the fractional level of contamination in the first two columns and the fraction of defective particles in the column headings. The body of the table gives the total release (sum) of both sources. The diagonal line through the table separates regions which are controlled by different sources of fission-product release: above the diagonal the release from defective particles is predominant, while below the line release from uranium contamination is controlling. It is clear that sensible specifications yield total releases that are close to the diagonal and are at levels that are considered reasonably attainable.

Studies are currently under way to determine individually the fractional release that results from (1) contamination in the coating, (2) defective particles, and (3) intact, high-quality TRISO particles. Although suitable experiments can be carried out to measure the releases due to contamination and defective particles, the intrinsic release from high-quality material can, at present, only be determined by difference,* if at all, or by release measurements on the silicon carbide-covered substrate after the outer pyrolytic carbon has been removed.

The release due to contamination in the coating can be studied by measuring the release of a small number (20 to 30) of intact, well-coated particles. The particles are radiographed to ensure that all layers of the coating are present and intact. Activation of the uranium, followed by oxidative destruction of the outer pyrolytic carbon coating and collection of the Xe^{135} released is used to determine the fraction of uranium in the pyrolytic carbon layers. The coating abrasion technique has been used, but to date it has not had sufficient sensitivity.

*The intrinsic release measured in this manner will be the difference between measurements on the intact high-quality particles and measurements of the contamination level.

Table V.8
ESTIMATED RELEASE OF METALLIC FISSION PRODUCTS
FROM TRISO-COATED FUEL PARTICLES

FRACTION OF DEFECTIVE PARTICLES:		0.1	0.01	0.001	0.0001	0.00001
FRACTIONAL RELEASE DUE TO DEFECTIVE PARTICLES: ^a		5×10^{-2}	5×10^{-3}	5×10^{-4}	5×10^{-5}	5×10^{-6}
FRACTIONAL CONTAMINATION IN COATING	FRACTIONAL ^b RELEASE DUE TO CONTAMINATION IN COATING	TOTAL RELEASE				
10^{-6}	10^{-6}	5×10^{-2}	5×10^{-3}	5×10^{-4}	5.1×10^{-5}	6×10^{-6}
10^{-5}	10^{-5}	5×10^{-2}	5×10^{-3}	5.1×10^{-4}	6×10^{-5}	1.5×10^{-5}
10^{-4}	10^{-4}	5×10^{-2}	5.1×10^{-3}	6×10^{-4}	1.5×10^{-4}	1×10^{-4}
10^{-3}	10^{-3}	5.1×10^{-2}	6×10^{-3}	1.5×10^{-3}	1×10^{-3}	1×10^{-3}
10^{-2}	10^{-2}	6×10^{-2}	1.5×10^{-2}	1×10^{-2}	1×10^{-2}	1×10^{-2}
2×10^{-2}	2×10^{-2}	7×10^{-2}	2.5×10^{-2}	2×10^{-2}	2×10^{-2}	2×10^{-2}
5×10^{-2}	5×10^{-2}	1×10^{-1}	5.5×10^{-2}	5×10^{-2}	5×10^{-2}	5×10^{-2}

^a THE STEADY-STATE FRACTIONAL RELEASE OF METALLIC FISSION PRODUCTS FROM A DEFECTIVE PARTICLE IS ASSUMED TO BE 0.5.

^b THE STEADY-STATE FRACTIONAL RELEASE OF METALLIC FISSION PRODUCTS FROM URANIUM CONTAMINATION IN THE OUTER PYROLYTIC CARBON COATING IS ASSUMED TO BE UNITY.

▨ DEFECTIVE PARTICLE CONTROLLED.

▧ CONTAMINATION CONTROLLED.

If activation and oxidative destruction studies are carried out on large representative samples of TRISO-coated fuel particles, the Xe^{135} released will result from both contamination and defective particles. Similar destructive oxidation studies on perfect particles will yield the fraction of defective particles by difference, although the necessity of using two samples and subtracting two possibly large numbers is not the most desirable technique.

In the past, oxidative destruction tests of some selected samples of particles used in the P13F and P13H capsules indicated that the fractions of uranium in the outer pyrolytic carbon coating and in defective particles was of the order of 10^{-4} to 10^{-3} . Postactivation annealing tests (for 8 hr at 1400°C) also yielded barium and strontium releases that tended to confirm these values. In most of these experiments a large number of particles (approximately 5000) was used, and complete microscopic analysis was not possible. However, in a few cases where only 20 to 30 irradiated particles were used and microscopic examinations of the particles were conducted, the particles were intact after burnoff experiments, indicating that the silicon carbide layers were intact. It was then assumed that the silicon carbide layers were also intact for the large samples (approximately 5000 particles) and the 10^{-4} to 10^{-3} release level was therefore attributed to contamination. This conclusion is subject to some uncertainty, however, since the small irradiated samples, which were intact, might have been contaminated in the capsule or the hot cell to a level of 10^{-4} to 10^{-3} while the true contamination level was considerably lower. The earlier assumption that the 10^{-4} level release from both large unirradiated samples and small irradiated samples was due to the same cause (i.e., contamination) was not necessarily valid.

Recently, however, a substantial amount of data has been acquired that tends to invalidate the earlier indication that contamination was the principal problem. A microradiographic analysis of a sample to be used in a fission-product release experiment revealed several interesting facts that are important in interpreting metallic fission-product release data:

1. The appearance of some of the silicon carbide layers indicated that defects may have been present (see Fig. V.19).
2. Some particles contained only an isotropic pyrolytic carbon layer surrounding the kernels (see Figs. V.20 and V.21).
3. Some dense material, which appeared to be kernel fragments, was present.
4. The outer pyrolytic carbon layer on some particles was absent (see Fig. V.20).

Particles with the above defects were classified as defective particles. These observations indicated that the major source of fission-product release may be defective particles rather than uranium contamination of the outer pyrolytic carbon layers, and further studies of these samples were carried out.

A method of partially separating defective particles from intact particles was devised. Some particles from lot 3332-101E were sieved, and the

Fig. V.19--Defective fuel particles showing half of buffer and SiC broken away and subsequently coated with PyC (200X)

Fig. V.20--Defective fuel particles showing two PyC-coated cores. Thicker coating on one particle shows buffer + PyC but no SiC (100X)

Fig. V.21--Defective fuel particles showing pyrolytic carbon-coated core and particle with no outer PyC (100X)

particles below 354 μm were further separated using the sink-float technique. The heavy fraction was microradiographed and found to contain a larger number of defective particles than the light fraction.

Postactivation fission-product annealing experiments were conducted on a defective fraction (eight particles) and an intact sample (ten particles). The strontium, barium, and xenon release fractions are given in Table V.9. For all the isotopes the defective particles showed a much greater release of fission products. It appears that the release from intact but contaminated particles is of the order of 10^{-5} and that much of this is due to the contamination. The intrinsic release is therefore probably insignificant.

Approximately 10 particles were used in these experiments, but in an earlier sample 12 defective particles (particles with only an isotropic pyrolytic carbon coating) were found in a 2-g sample (about 20,000 particles). The twelve defective particles (in 20,000) with a fractional release of 0.5 will yield a minimum fractional release of 3×10^{-4} for the sample.

The measurements discussed above are strong, though not conclusive, evidence for the premise that the observed fission-product release from TRISO-coated particles is due almost entirely to contamination and defective particles. Recent measurements of strontium diffusion in silicon carbide indicate that the diffusion coefficient for strontium is only one to two orders of magnitude lower than in pyrolytic carbon. The delay time for diffusion through a 20- μm silicon carbide layer is therefore about 1 hr (using the approximation $x^2 = 6 D_{\text{Sr}} t$, where $x = 20 \mu\text{m}$, $D_{\text{Sr}} = 3 \times 10^{-10} \text{ cm}^2 \text{ sec}^{-1}$, and t is the time in seconds). Thus, during an 8-hr diffusion anneal the steady-state release is closely approximated. The low levels of release that are observed, therefore, indicate that strontium transport is limited by the low solubility of strontium in silicon carbide rather than by a low diffusion coefficient. Further, these low release rates should prevail throughout the life of the reactor assuming that the silicon carbide layers remain intact.

DIFFUSION COEFFICIENTS OF BARIUM AND STRONTIUM IN SILICON CARBIDE

Attempts were made to determine diffusion coefficients for barium and strontium in SiC from the results of postactivation fission-product release measurements on TRISO-coated particles. Release tests were conducted at 1350°, 1550°, and 1800°C on six samples, each of which contained about 20 particles (three samples from lot 3332-101E and three from 3592-53E). The samples were mixed with charcoal to act as a sorbent for the metallic fission products and were contained in closed tantalum crucibles. Microradiographs of the samples were taken before irradiation and after about 680 hr of annealing at the respective temperatures. The Ba^{140} released was measured at various intervals during the anneals. Only the sample annealed at 1550°C from lot 3592-53E showed no measurable release of barium ($<10^{-4}$ fraction), whereas the sample from lot 3332-101E annealed at 1550°C and the samples annealed at 1350° and 1800°C showed significant barium loss. Oxidative destruction (burnoff) of the outer PyC layers of the releasing samples after the anneals confirmed SiC-coating failure in each of the samples after the 680-hr anneals. Thus, the barium released could be attributed to failed particles. The release fraction

Table V.9

FRACTIONAL RELEASE FROM BARRIER-COATED FUEL PARTICLES AT 1400°C

Lot No. and Description	Coating Thickness (μm)			Anneal Time (hr)	Fractional Release ^a		
	Buffer	Carbide	Isotropic		Xe ¹³⁵	Ba ¹⁴⁰	Sr ⁹¹
3332-101E, 8 defective particles	58	13	40	Recoil ^b	---	---	---
				1	---	1.0×10^{-1}	3.6×10^{-1}
				3	---	1.6×10^{-1}	4.9×10^{-1}
				5	1.3×10^{-4}	1.9×10^{-1}	5.0×10^{-1}
				8	---	2.1×10^{-1}	5.3×10^{-1}
3332-101E, 10 intact particles	58	13	40	Recoil ^b	---	---	---
				1	---	---	---
				3	---	---	---
				5	$< 1.4 \times 10^{-6}$	---	---
				8	---	$< 2.9 \times 10^{-5}$	$< 3.0 \times 10^{-5}$

^aSamples irradiated for 1 hr in TRIGA King furnace at 1400°C to determine steady-state releases of Kr and Xe. The data above do not include release of fission products during irradiation. Total release fractions are greater than those given in table.

^bRecoil release as determined by Xe¹³⁵ present in the irradiation chamber.

for Xe^{135} obtained during burnoff of the sample from lot 3592-53E annealed at 1550°C was relatively low (less than 2×10^{-5}). The failures are attributed to unforeseen experimental difficulties.

These experiments probably would not have obtained the desired results even if coating failure had not occurred. Recent data have indicated that the strontium diffusion coefficients are only about two orders of magnitude lower in SiC than in PyC. Therefore, the low level of transport of metals across the SiC layers is probably the result of the low solubility of strontium in SiC rather than a low diffusion coefficient. Using the recent strontium diffusion coefficients and calculating delay times corresponding to temperatures of 1550° and 1800°C for a $10\text{-}\mu\text{m}$ SiC barrier (3592-53E), lag times of 3 min at 1550°C and 0.3 min at 1800°C are obtained. The corresponding barium lag times would only be slightly longer.

PHASE BEHAVIOR OF THE Th-Si-C SYSTEM

The study of the ternary Th-Si-C system utilizing the thermal analysis technique has continued. Current thermal analysis data show the possible existence of a liquid phase at about 2300°C . Preliminary X-ray analyses of these samples indicates the presence of only two phases, ThC_2 and $\alpha\text{-SiC}$. Since ThC_2 melts at 2550°C , the phase melting at about 2300°C as shown by thermal analyses is most likely $\alpha\text{-SiC}$ rather than a low-melting eutectic.

In addition to the X-ray analysis, a diffusion anneal was conducted between SiC and ThC_2 . The diffusion couple was made by pressing a graphite disk coated with about $300\text{ }\mu\text{m}$ of SiC between two freshly prepared ThC_2 pellets. One side of the SiC-coated disk was adjacent to a thin disk of buffer carbon sandwiched between the ThC_2 pellet and the SiC-coated disk. The buffer carbon disk was approximately $1000\text{-}\mu\text{m}$ thick. The various pieces of the anneal were pressed together in a screw-top AGOT graphite crucible. The sample was annealed for 111 hr in vacuum at 1550°C .

Examination of the sample after annealing indicated only slight interaction between the SiC and ThC_2 . The reaction that did occur was at the interface between the SiC and ThC_2 sections, which were in direct contact. Metallographic examination of the component parts of the anneal shows no interdiffusion between the SiC and ThC_2 , although the SiC disk appeared to have vaporized to some extent. A second diffusion anneal is currently in progress, using ThC_2 pellets, a SiC-coated disk, and a thin disk of buffer carbon. This anneal will be conducted at about 1500°C for 500 hr.

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