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AEC RESEARCH AND
DEVELOPMENT REPORT

GA-10010
UC-80 Reactor
Technology

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

QUARTERLY PROGRESS REPORT
FOR THE PERIOD ENDING
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INTRODUCTION

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

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

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PART I

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

Task I

COMPONENT DEVELOPMENT

THERMAL BARRIER

The objective of this subtask is to establish the suitability and reliability 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).

Tests performed by Bertin and Company on the Class B thermal barrier for the core support floor duct have been completed. The test fixture is shown in Fig. 1.1. The cylindrical section of the fixture was divided into seven zones cooled by separate cooling circuits. The inlets and outlets of the circuits were grouped between adjacent zones to limit heat transfer by conduction between zones. The areas and mean temperatures in the end zones (1 and 7) were found to be difficult to define; the heat flux was therefore measured for each zone except these.

All the tests performed on the Class B thermal barrier are presented in Table 1.1. The initial tests (Tests 1C through 8C) were performed without forced helium circulation and were therefore referred to as natural-convection tests. A summary of these tests has been plotted in Fig. 1.2 in the form of heat flux versus zone for various pressures and a hot-face temperature of 1500°F. As shown by this plot, a considerable increase in heat flux was experienced when the pressure was increased. In addition, the average heat flux at the higher pressures was considerably greater than expected. The forced-convection tests (tests 11A through 13D) also yielded heat fluxes that were higher than expected. Tests 14C, 14E, and 15A were conducted by Bertin to determine whether the unexpected results were due to convection currents in the insulation set up by leakage through holes in the hot face. The holes had been drilled in the hot face to simulate failure of the thermal barrier's internal bellows seal. As shown in Fig. 1.3, heat transfer with the holes sealed was still excessive.

Permeability tests were therefore performed on the test fixture. The results of these tests indicated that the high heat-transfer rates were due to leakage at the upper flange (see Fig. 1.1). Leakage in this

Fig. 1.1--Test fixture for Class B thermal barrier tests

Table 1.1
CLASS B THERMAL BARRIER TESTS

Test No.	Convection Type	Req'd Temp. (°F)	Press. (psi)	ΔP (psi)	Heat Flux (Btu/ft ² -hr)	
					Min.	Max.
Initial results, leakage at upper flange						
1C	Natural	1501	14.50	--	895	1160
1D	Natural	1501	14.50	--	901	1210
2E	Natural	1150	14.50	--	622	725
2G	Natural	1150	14.50	--	634	685
3D	Natural	801	14.50	--	361	412
3E	Natural	801	14.50	--	330	483
4C	Natural	801	719	--	533	733
4E	Natural	801	718	--	582	702
5G	Natural	1150	711	--	1000	1195
5L	Natural	1150	711	--	992	1225
6A	Natural	1501	710	--	1570	1850
6C	Natural	1501	697	--	1590	1845
7B	Natural	1501	509	--	1570	1785
7E	Natural	1501	505	--	1520	1800
8C	Natural	1501	302	--	1485	1695
11A	Forced	401	682	0.4	840	1240
11B	Forced	407	682	0.4	868	1245
12B	Forced	539	698	0.2	710	1145
12D	Forced	538	698	0.2	760	1240
13B	Forced	558	700	0.17	712	1184
13D	Forced	559	700	0.17	720	1185

Table 1.1 (Continued)

Test No.	Convection Type	Req'd Temp. (°F)	Press. (psi)	ΔP (psi)	Heat Flux (Btu/ft ² -hr)	
					Min.	Max.
Permeability tests, holes sealed, leakage at upper flange						
14C	Forced	557	718	0.39	1025	1665
14E	Forced	569	711	0.32	988	1630
15A	Forced	476	690	0.6	1070	1640
Upper flange sealed with sealant, holes sealed						
16B	Forced	392	710	0.4	281	361
17E	Forced	356	702	0.2	222	267
18B	Forced	572	689	0.6	511	842
19C	Forced	582	692	0.4	532	772
19D	Forced	563	692	0.4	495	745
20C	Forced	733	695	0.8	775	1320
20D	Forced	733	702	0.8	812	1370
21B	Forced	752	702	0.6	800	1260
21D	Forced	771	702	0.6	809	1255
22C	Forced	802	700	0.4	793	1180
22D	Forced	801	698	0.4	791	1175
Upper flange welded, hot-face holes sealed						
23D	Forced	801	700	0.4	660	892
23F	Forced	801	696	0.4	640	885
24B	Forced	801	710	0.6	713	900
24C	Forced	801	696	0.6	630	870
25B	Forced	801	696	0.8	605	880
25D	Forced	801	696	0.8	639	858

Table 1.1 (Continued)

Test No.	Convection Type	Req'd Temp. (°F)	Press. (psi)	ΔP (psi)	Heat Flux (Btu/ft ² -hr)	
					Min.	Max.
Upper flange welded, hot-face holes open						
26B	Forced	801	700	0.2	655	846
26D	Forced	801	700	0.2	645	842
27C	Forced	801	696	0.35	659	823
27E	Forced	801	696	0.35	614	810
28A	Forced	801	696	0.6	679	1010
29A	Forced	801	690	0.8	719	945
30A	Natural	410	16	--	131	150
30C	Natural	410	16	--	130	155
31B	Natural	801	16	--	361	380
31E	Natural	801	16	--	380	437
32C	Natural	1130	16	--	632	745
32E	Natural	1148	16	--	613	741
33B	Natural	1321	16	--	972	1185
33E	Natural	1321	16	--	1015	1200
34B	Natural	1321	696	--	1540	1720
34D	Natural	1321	696	--	1570	1750
35C	Natural	801	720	--	544	611
35D	Natural	801	720	--	555	615

Fig. 1.2--Class B thermal barrier heat flux versus zone for a hot-face temperature of 1501°F natural convection. Tests 1C, 1D, 6A, 6C, 7B, 7E, 8C

Fig. 1.3--Class B thermal barrier heat flux versus zone for a hot-face temperature of 560°F and a test pressure of 700 psi with forced convection

area allowed natural convection currents to be set up in the gap between the cold liner and the insulation. At increased pressures this effect was intensified because of the increased density of the helium. The leakage was found to be particularly important in the case of forced convection, since the pressure differential established between the top and bottom heads increased the flow through the flange. Initial attempts to seal the flange with rubber sealant (tests 16B through 22D) yielded lower heat-transfer rates, but still did not seal the flange completely. To eliminate any possibility of leakage in this area, the flange was welded. Data obtained from tests with the flange welded are presented in Table 1.2.

The results of forced-convection tests run with the flange welded and the hot-face holes sealed are shown in Fig. 1.4. Note that although the cooling circuits were arranged to eliminate the conduction between zones, the heat flux in Zones 2 and 6 was much higher. This was felt to be due to axial conduction from the top and bottom of the test rig. Therefore, an average heat flux was calculated only for data from Zones 3, 4, and 5. Figure 1.5 is a plot of average heat flux versus pressure differential for forced-convection tests run with the hot-face holes sealed and open. Data points marked with an asterisk are based on initial measurements taken for a particular test run and possibly do not represent a true steady-state condition. As shown on this plot, the leakage through the holes in the hot face do not create much of an effect until the pressure differential has reached approximately 0.4 psi. The pressure differential expected in the core support floor duct is approximately 0.5 psi. At this ΔP , the test data indicated that the heat flux would increase approximately 3% if the thermal barrier inner shell were not completely sealed.

The results of the natural-convection tests with the flange welded and the holes open are plotted in Fig. 1.6. The average heat flux has been plotted as a function of the temperature differential across the insulation and liner ($T_{\text{hot face}} - T_{\text{cold face}}$). Data are shown for the two pressures at which the tests were run, 16 psi and 700 psi. It appears from the results of the forced- and natural-convection tests, that natural-convection currents exist in the annulus between the liner and the insulation. These natural-convection effects would increase with increasing ΔT and increasing density, which would explain the increased slope of the 700-psi curve.

Temperature limitations of the blower motor used in the test fixture prevented forced-convection tests from being performed above approximately 800°F. The Class B insulation in the core support floor duct is designed to operate at a ΔT of 1300°F. To obtain forced-convection data at this ΔT , the forced-convection data from Fig. 1.5 was replotted on Fig. 1.6 and extrapolated along the slope of the natural-convection curve. The forced-convection data are based on a pressure differential of 0.5 psi, which corresponds to the expected ΔP in the steam generator cavity. As

Table 1.2

RESULTS OF CLASS B THERMAL BARRIER TESTS WITH UPPER FLANGE WELDED

Test No. and Condition	T _{hot} (°F)	P (psi)	ΔP (psi)	ΔT (°F)	Heat Flux (Btu/hr-ft ²)					
					Zone 2	Zone 3	Zone 4	Zone 5	Zone 6	Average
Forced Convection, Hot-face holes sealed:										
23D	810	700	0.4	701	892	680	660	745	842	695 ^a /764
23F	795	696	0.4	687	885	640	703	699	805	681 ^a /746
24B	790	710	0.6	692	870	713	725	714	900	717 ^a /784
24C	790	696	0.6	681	870	630	740	693	838	688 ^a /754
25B	790	696	0.8	679	880	605	653	793	782	684 ^a /743
25D	790	696	0.8	679	858	639	720	790	835	716 ^a /768
Forced Convection, Hot-face holes open:										
26B	785	700	0.2	676	846	655	681	701	775	679 ^a /732
26D	785	700	0.2	674	842	645	668	701	751	671 ^a /721
27C	792	696	0.35	679	823	659	657	709	792	675 ^a /728
27E	802	696	0.35	684	810	703	614	726	759	681 ^a /722
28A	797	696	0.6	689	1010	679	746	771	895	732 ^a /820
29A	791	690	0.8	685	869	719	771	789	945	760 ^a /817

Table 1.2 (Continued)

Test No. and Condition	T _{hot} (°F)	P (psi)	ΔP (psi)	ΔT (°F)	Heat Flux (Btu/hr-ft ²)					
					Zone 2	Zone 3	Zone 4	Zone 5	Zone 6	Average
Natural Convection, Hot-face holes open:										
30A	416	16	--	314	139	131	142	142	150	138 ^a /141
30C	410	16	--	308	132	144	137	130	155	137 ^a /140
31B	799	16	--	697	378	378	414	361	380	384 ^a /382
31E	802	16	--	696	394	380	392	385	437	386 ^a /398
32C	1137	16	--	1017	641	660	679	632	745	657 ^a /671
32E	1137	16	--	1015	646	650	661	613	741	641 ^a /662
33B	1507	16	--	1378	1020	1040	1080	972	1185	1031 ^a /1059
33E	1517	16	--	1388	1065	1070	1100	1015	1200	1062 ^a /1090
34B	1502	696	--	1369	1720	1580	1570	1540	1620	1563 ^a /1606
34D	1502	696	--	1371	1750	1600	1600	1570	1650	1590 ^a /1634
35C	801	720	--	692	611	559	561	544	565	555 ^a /568
35D	801	720	--	692	615	575	555	555	582	562 ^a /576

^aAverage heat flux over zones 3, 4, and 5.

Fig. 1.4--Class B thermal barrier heat flux versus zone for a hot-face temperature of 790°F and a test pressure of 696 psi with forced convection

Fig. 1.5--Class B thermal barrier average heat flux versus pressure difference for a hot-face temperature of 790°F and a test pressure of 700 psi with forced convection

Fig. 1.6--Class B thermal barrier average heat flux versus temperature difference with natural convection

shown in Fig. 1.6, the test data give an average heat flux of 1610 Btu/ft²-hr compared with a design heat flux of 1152 Btu/ft²-hr. One factor that contributed to the higher-than-expected heat flux was that the insulation on the test fixture was approximately 5.6% thinner than the insulation used in the plant. Adjusting the test data by this factor would yield an average heat flux of 1530 Btu/ft²-hr. It should also be noted that the height of the test fixture was slightly shorter than the core support floor duct thermal barrier. This factor would result in higher axial heat leaks in the test fixture, but the magnitude of the effect is difficult to account for.

The following conclusions were reached in analyzing the Class B thermal barrier tests:

1. Any leakage of helium through the annulus between the liner and insulation will cause excessively high heat-transfer rates and will defeat the purpose of the thermal barrier. (This fact has resulted in design changes in the core support floor duct thermal barrier to eliminate the possibility of leakage.)
2. Helium leakage through breaks in the inner shell has a small effect on heat flux for the pressure differential expected in the reactor.
3. Even if all leakage through the annulus between the liner and the insulation is eliminated, natural-convection effects will still be present in that open annulus. These effects are probably responsible for the higher-than-expected heat flux calculated from the test data.
4. The test data yielded an average heat flux of 1530 Btu/ft²-hr for the Class B insulation compared to a design heat flux of 1152 Btu/ft²-hr. The larger heat flux obtained from the test will be used to update design calculations and the expected heat load tabulation for the cooling system. It should be noted that the cooling system capacity in this area is more than 2 times the test result.

AUXILIARY EQUIPMENT AND SPECIAL TOOLS

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

Reserve Shutdown Vacuum Tool

On the basis of engineering calculations and confirmation by the test results presented in an earlier quarterly report, GA-9720, an order has been placed for the blower of the reserve shutdown vacuum tool. The

blower, which is a regenerative turbine-type compressor (Ref. 1), has the following characteristics:

Capacity.	800 cfm of helium
Inlet pressure.	10.70 psia
Exhaust pressure.	12.5 psia
Ambient pressure.	12.5 psia
Inlet temperature	250°F
Ambient temperature	250°F
Duty cycle.	4 min operating, 2 hr non- operating, for 70 operat- ing cycles
Speed	24,000 rpm
Input power	20 kw, 440 volt, 400 cps, 3 phase

The rotating parts assembly of a similar regenerative turbine type compressor are shown in Fig. 1.7.

Power to the blower is provided by a motor-alternator set operating on a 480-v, 60-cps, 3-phase supply and producing a 440-v, 400 cps, 3-phase output. The pressure drop versus flow characteristics, including a comparison of calculated and test results and specification data, are shown Fig. 1.8.

An evaluation of the possible modes for failure of the blower produced the following results:

1. All probable electrical modes of failure relating to the blower are adequately covered by the motor overload protection contained within the motor-alternator set.
2. Past experience has shown that, in the event the compressor wheel bursts, the wheel fragments would be totally contained within the compressor housing.
3. Should an internal seizure cause the compressor wheel to decelerate from full speed to zero speed in 0.5 sec, a torque of 260 ft-lb would be imparted to the blower frame. This torque would exert a reaction of approximately 100 lb on each of the 1/2-in. attachment bolts.

Special Tools

Testing of the special tools in air was initiated and partially completed. The core service tools all operate as a part of or in conjunction with the core service manipulator, which is mounted on the fuel

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

Fig. 1.7--Rotating parts assembly

Fig. 1.8--Reserve shutdown vacuum tool blower flow characteristics pressure drop versus flow

handling machine in place of the normal fuel handling grapple head. Each tool is designed for particular functions, and each tool was tested to determine its ability to perform. The five tests and their results are described below.

1. Pick up miscellaneous pieces of graphite with the 5-in. hand on the manipulator. This was accomplished by simulating the technique that is intended for use under actual circumstances. That is, the debris is located visually and a detailed procedure is then developed for the removal of each piece by using a mockup, guiding the tool into the simulated piece, and then recording each successful step. The process is then repeated by the operator without assistance using the procedure thus developed.
2. Remove small graphite fragments and dust with the core service vacuum cleaner. The vacuum cleaner functioned very well; however, it was determined that a minor modification to its handle would be required to provide a better grip for the manipulator hook.
3. Remove a layer of simulated fuel blocks with the element pickup probe tool. The probe functioned as designed, but interference between the manipulator shoulder housing and the wall of elements formed by the simulated adjacent region prevented engagement of the probe with the handling hole of the elements next to the wall. Further testing was postponed to permit redesign and rework to eliminate the interference.
4. Right an element lying on its side by using the 8-in. hand on the manipulator. It was shown that insufficient clear space existed for the 8-in. hand to function as intended. An adaptor to the 8-in. hand was therefore designed and is being fabricated. After fabrication the test will be repeated to demonstrate the capability of righting an element.
5. Remove a control rod with a failed cable. This test consisted of three parts: (1) cutting of the cable, (2) retrieval of the cable, and (3) removal of the control rod. Of these three parts, only the removal of the control rod with the control rod removal tool was successfully performed. The cable cutoff tool was found to function properly when the cable was properly located in the tool; however, under remote operating condition, proper placement without further modifications was not considered feasible. Studies are under way to determine the modifications required.

Reference

1. ASME publication 64-WA/FE-25.

Task II
NUCLEAR ANALYSIS

FUEL CYCLE ANALYSIS

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

Status of Nuclear Design

The design calculations for the Reference Design 10 initial core were completed. After reviewing all uncertainties applicable to the initial core excess reactivity, a small reduction of the initial core thorium loading was necessary to ensure an adequate reactivity lifetime for the initial cycle.

As a result of this design iteration, a preliminary specification was issued for the initial core fissile and fertile material loadings. As established during several design iterations, the initial core will contain 13 fuel compositions, 12 of which are used to mock up the equilibrium cycle age distribution and to provide radial and axial fuel zoning. The thirteenth composition is contained in five rows of fuel holes at the core-reflector interface which act as a buffer zone against inleaking neutrons from the side reflector.

FUEL ZONING AND POWER SHAPING

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

Reference Design 10

The evaluation of uncertainties applicable to the excess reactivity in the initial core led to a slight modification of the initial core loadings compared to Reference Design 9. The thorium loading for the initial cycle was reduced by approximately 1.5% in order to ensure a reactivity lifetime of about 300 full-power days for the first core. This loading reduction increases the initial core excess reactivity by about $0.005 \Delta k$.

As established in previous design iterations, Reference Design 10 utilizes 13 fuel compositions for the initial core. Analysis to date has shown that this number of fuel compositions is necessary to provide acceptable power and temperature distributions by radial and axial fuel zoning and to provide an economic fuel cycle by partially mocking up the equilibrium cycle fuel age distribution. Table 2.1 summarizes the total amounts of fissile and fertile material in the different fuel compositions. The total initial core loading for the Reference Design 10 is 878 kg of 93.15% enriched uranium and 19,166 kg of thorium. The fuel loading densities that are obtained when distributing the specified material into the available fuel hole volume are summarized in Table 2.2. Uranium densities vary from about 0.08 to 0.16 g/in., whereas thorium densities cover the range between 1.8 and 2.9 g/in. The actual locations of the various fuel compositions within the active core are shown in Fig. 2.1.

Reference Design 10 utilizes a five-fuel-hole-thick buffer zone at the core-reflector interface (fuel composition 13). Previous analyses (see earlier quarterly report GA-9720) have shown that an extension of this buffer zone to include a complete fuel element is not as attractive as the reference design with regard to fuel temperatures.

Cross sections and carbon densities are based on blended fuel beds; however, as pointed out in the previous quarterly report, GA-9879, the calculated reactivity difference between a blended bed and bonded rod system is less than $0.005 \Delta k$. The fuel element structure carbon density was assumed to be 1.78 g/cm^3 , a value close to that measured during the production of raw graphite logs.

Two kinds of impurities may be present within the core: hydrogenous materials such as water or hydrocarbons, and parasitic absorbers such as boron or its equivalent. The hydrogenous materials are present primarily as additional material within the fuel rod, such as pitch or binder in a bonded rod or coke particles in a blended fuel bed. At the present time, 40 to 100 lb of hydrogen are anticipated in the initial core. The reactivity worth of this hydrogen is substantial (50 lb of hydrogen are worth $+0.01 \Delta k/k$), but most of it will be removed the first time the reactor is brought to operating temperature. In the analysis for determining the core excess reactivity, it was therefore assumed that all of the hydrogen had been removed from the core.

The parasitic impurity level for all of the graphite fuel elements was required by specifications to be less than 5 ppm boron equivalent, which is worth about $0.02 \Delta k/k$. However, the actual amount of boron or its parasitic equivalent present in several samples was shown by chemical analysis to be less than 1 ppm. It was therefore assumed for this analysis that the burnable impurity in the fuel element graphite was 1 ppm boron (0.2 ppm B^{-10}) and that the nonburnable impurity, or boron equivalent, was 0.1 ppm natural boron. In addition, impurities were assumed to be present in the fuel particles and fuel bed. Their nominal values are summarized in Table 2.3.

Table 2.1
INITIAL CORE FUEL LOADINGS
(Reference Design 10)

Fuel Composition	Metal Loading (kg)	
	Uranium	Thorium
1	120.7	3516
2	92.2	3143
3	44.3	761
4	32.7	651
5	100.5	1581
6	74.6	1385
7	128.1	1790
8	97.0	1593
9	40.9	861
10	29.2	716
11	38.9	714
12	28.8	617
13	50.7	1838
Total	878.0	19166

Table 2.2
DEFINITION OF FUEL LOADING DENSITIES
FORT ST. VRAIN INITIAL CORE
(Reference Design 10)

Fuel Composition (blend)	Metal Loading (g/in.)	
	Uranium	Thorium
1	0.0873	2.542
2	0.0673	2.292
3	0.1231	2.114
4	0.0916	1.824
5	0.1396	2.196
6	0.1043	1.937
7	0.1578	2.204
8	0.1205	1.979
9	0.1136	2.392
10	0.0816	2.006
11	0.1441	2.644
12	0.1075	2.302
13	0.0791	2.867

Fig. 2.1--Initial core fuel loading distribution

Table 2.3
FUEL ELEMENT IMPURITIES
(Total boron equivalents)

Impurity	Impurity Level (ppm by weight)			
	Structural Graphite	Fuel Kernels	Particle Coating	Coke Particles
B^{10} (burnable)	0.2	0.4	0.1	0.2
B_{nat} (nonburnable)	0.1	0.4	0.1	0.1

Table 2.4
REACTIVITY UNCERTAINTIES IN FORT ST VRAIN REACTOR

Core Material Content:

Uranium + thorium loading $\pm 0.002 \Delta k$
Carbon loading $\pm 0.002 \Delta k$
Impurities (excluding hydrogen). $\pm 0.005 \Delta k$

Analytical Model:

Calculation of grain shielding $\pm 0.002 \Delta k$
Lumped poison worth (5% of total). $\pm 0.002 \Delta k$
Modeling core geometry $\pm 0.005 \Delta k$

Cross Sections:

Basic data and thorium resonance integral. $\pm 0.007 \Delta k$
Temperature defect (10% of total). $\pm 0.007 \Delta k$
Total, hot clean (mean square). $\pm 0.015 \Delta k$

It is anticipated that the initial core will contain about 0.05 $\Delta k/k$ of reactivity control in the form of lumped burnable poison rods. Every fuel element, except those containing fuel composition 1 or 2 and those in which the control rods are located, will contain two poison rods. They are located in the corners of the elements as described earlier in quarterly report GA-8879 and are uniform with respect to the upper and lower halves.

The cylindrical poison rods consist of boron carbide in a graphite matrix having an outer diameter of 0.405 in. Both the amount of boron present in the rod and the outer diameter of the rod are subject to some change to best meet the reactivity requirements of the core during the first year. The B^{10} loading for these rods as contained in the present design was 0.0215 g of boron per cubic centimeter of poison rod.

To predict the reactivity worth of these lumped poison rods as well as the depletion of the poison during reactor operation, accurate effective cross sections are required. The B^{10} cross section averaged over the average core spectrum is used in conjunction with self-shielding factors as a function of $(\Sigma_{ar})_{rod}$. The analytical treatment of burnable poison self-shielding factors was discussed in GA-8879.

The general method for calculating the reactivity worth of lumped poison rods is to homogenize the poison density uniformly in a fuel element and to apply the appropriate flux self-shielding factors. Analysis indicates that the reactivity worth of the poison as a function of reactor burnup using this model agrees closely with that using a model with more explicit representation (see earlier quarterly reports GA-8879 and GA-9720). This is true even for those poison rods located near the outer boundary of the core. Consequently, this model was used for the depletion analysis of the initial core.

Reactivity Uncertainties. The reactivity uncertainties applicable to the initial core beginning-of-cycle excess reactivity have been reviewed and are summarized in Table 2.4. The listed data are based upon a variety of analyses performed during the last few years and upon loading and design tolerances, knowledge of basic cross-section data, and experience obtained at the Peach Bottom reactor during the initial core physics tests and subsequent power operation.

Calculational Methods. Most of the analysis for Reference Design 10 was performed using two-dimensional diffusion theory calculations and single-channel synthesis methods. Specifically the GAUGE (Ref. 1), SCANAL (Ref. 2), BUGTRI (Ref. 3), and GAMBLE (Ref. 4) codes were used to model the core geometry and fuel loading distribution.

Preliminary reactivity estimates were obtained with the GAUGE code. This diffusion code is very fast (about 1 min per diffusion calculation), which makes it a prime tool for obtaining good and rapid estimates. However,

several disadvantages have to be taken into account. First, an average axial loading density has to be used for all core regions, neglecting the axial fuel variations and their reactivity effect. Second, the minimum mesh size is restricted to the corner length of one fuel element, which makes it impossible to represent the buffer zone at the core-reflector interface. Finally, its use is restricted to one thermal-neutron energy group. This limitation is especially disadvantageous when calculating flux distributions near fuel discontinuities and at the core-reflector interface where the neutron energy spectrum changes rapidly with distance; however, as will be shown below, fairly good results are obtained despite these limitations if the effective thermal cross sections are carefully selected. In particular, refueling region peaking factors, the prime reactor parameters for the thermal design, are predicted fairly accurately (to within $\pm 10\%$).

Better reactivity estimates are obtained with the SCANAL code, a single-channel synthesis package. The use of two core slices, representing the top and bottom halves of the core, appears to produce an approximation that is sufficiently accurate for taking the axial flux variations into account. The SCANAL code, however, has the same disadvantages as the GAUGE code with respect to mesh size and neutron energy group structure, and region power peaking estimates are probably no more accurate.

The best (i.e., most accurate) reactivity estimates are obtained with the BUGTRI and GAMBLE codes. Both allow up to 10 neutron energy groups and use a considerably finer mesh size than the codes mentioned above. The BUGTRI code, having a triangular mesh, allows an excellent representation of the actual core and reflector geometry, including the buffer zone at the core-reflector interface.

In order to obtain a best estimate for the initial core excess reactivity and control rod worth, seven-group calculations (three fast, four thermal groups) were performed for the top and bottom half of the core with a mesh spacing equivalent to one half of the corner length of a fuel element. Subsequently, flux- and volume-weighted macroscopic cross sections were calculated for rings equivalent to the rings of refueling regions for both the top and bottom core halves. These macroscopic cross sections were used in an RZ model to calculate the best estimate of the initial core reactivity. Detailed results and comparisons of various data are discussed below.

The next logical step to improve the core modeling is the use of three-dimensional calculations. The three-dimensional diffusion theory code GATT (Ref. 5), which is particularly suitable for the hexagonal fuel elements and the refueling region core structure, has been under development at Gulf General Atomic Incorporated for two years. An improved preliminary production version is now available to give a more or less absolute check on the above-mentioned approximations, and three-dimensional calculations with this code are planned.

Results. The calculated effective multiplication during the initial cycle for the hot operating core is shown in Fig. 2.2. The minimum excess reactivity with equilibrium xenon, samarium, and Pa^{233} occurs after about 60 days of full-power operation and at the end of the cycle. Between the two minima, the reactivity rises slightly when the burnable poison depletes more rapidly than the fissile material. Figure 2.2 also shows the applicable maximum reactivity uncertainty range indicating that another reactivity adjustment may be necessary if the reactivity uncertainties cannot be reduced.

The reactivity worth of the control rod bank calculated from this analysis with BUGTRI-RZ calculations is $0.223 \Delta k$. This bank worth does not include the fact that the control rod consists of discrete poison segments with gaps between the poison bodies. If these gaps are taken into account, the rod worths are reduced by about 3% to 5% (see earlier quarterly report GA-8038).

A summary of the calculated radial power distribution during the first year for the expected control rod patterns is given in Fig. 2.3. It can be seen that the maximum power density in any refueling region is always less than 1.83 of the core average except at two days, when a 13-control-rod bank is replaced with a 7-control-rod bank. At five days, after the equilibrium xenon is redistributed, the peaking factor has dropped to 1.60. This sudden exchange of control rods does not really occur, and consequently, the radial peaking factor is within the design limits of 1.83.

Comparison of Computational Methods. The effective multiplication for the two core slices and the total core for 0, 50, and 310.5 days of full-power operation as calculated with SCANAL and BUGTRI are compared in Table 2.5. The table shows that BUGTRI predicts a multiplication constant over $0.5\% \Delta k$ higher than SCANAL for the top slice at 0 and 50 days. Results for the bottom slice, however, differ by only about $0.1\% \Delta k$ at the beginning of the cycle and for 50 days; at 310.5 days of full-power operation, BUGTRI eigenvalues are less than those calculated from SCANAL. This is an indication that the very good agreement observed between BUGTRI and SCANAL is, to some extent, a cancellation of differences.

Table 2.6 compares the eigenvalues obtained from a SCANAL and a BUGTRI depletion analysis of the top slice. Here very sizeable differences are observed. It was originally believed that this change in reactivity differences from $1.0\% \Delta k$ at the beginning of the cycle to $0.3\% \Delta k$ at the end of the cycle was related to spectrum softening with depletion. A comparison of the beginning-of-cycle and end-of-cycle four-group thermal flux did not bear this out. Since rods are withdrawn and burnable poison is burned out with depletion, the rod and burnable poison absorption fractions were investigated as a possible cause of the shift in the reactivity difference with depletion. As can be seen in Table 2.7, the lumped burnable poison and control rod thermal absorption fraction is about 10% higher for SCANAL than for BUGTRI. The shift in the eigenvalue difference would therefore seem to lie in the treatment of the burnable poison and control rods.

Fig. 2.2--Effective multiplication versus time for initial cycle - Reference Design 10

Fig. 2.3--Refueling region peaking factors for Reference Design 10

Table 2.5
COMPARISON OF UNRODDED MULTIPLICATION AS CALCULATED
BY SCANAL AND BY BUGTRI-RZ
(Reference Design 10)

Position	k_{eff} 0 Days		k_{eff} 50 Days		k_{eff} 310.5 Days	
	SCANAL	BUGTRI-RZ	SCANAL	BUGTRI-RZ	SCANAL	BUGTRI-RZ
Top slice	1.0871	1.0930	1.0334	1.0417	1.0322	1.0355
Bottom slice	1.0427	1.0443	1.0033	1.0048	1.0093	1.0077
Total core	1.0524	1.0554	1.0040	1.0083	1.0050	1.0050

Table 2.6
COMPARISON OF RODDED TOP SLICE DEPLETION CALCULATIONS
BY SCANAL AND BY BUGTRI

Time (Days)	Rod Pairs Inserted	k_{eff} , SCANAL	k_{eff} , BUGTRI	Δk BUGTRI-SCANAL
0	13	1.0270	1.0370	0.0100
2	13	0.9940	1.0036	0.0096
2	7	1.0189	1.0260	0.0071
5	7	1.0142	1.0208	0.0066
10	77	1.0098	1.0165	0.0067
10	4	1.0220	1.0279	0.0059
50	4	1.0095	1.0136	0.0041
103.5	4	1.0125	1.0152	0.0027
207	4	1.0110	1.0118	0.0008
310.5	4	1.0062	1.0094	0.0032

Table 2.7

ABSORPTION FRACTIONS FOR TOP SLICE

	BUGTRI			SCANAL		
	Fast	Thermal	Total	Fast	Thermal	Total
LBP ^a	2.52×10^{-3}	2.43×10^{-2}	2.68×10^{-2}	2.45×10^{-3}	2.70×10^{-2}	2.94×10^{-2}
Rods	2.51×10^{-2}	1.94×10^{-2}	4.45×10^{-2}	2.55×10^{-2}	2.12×10^{-2}	4.67×10^{-2}
U-235	9.70×10^{-2}	4.46×10^{-1}	5.43×10^{-1}	9.47×10^{-2}	4.44×10^{-1}	5.39×10^{-1}

^aLumped burnable poison.

It may be mentioned here that, at the end of the cycle, the depleted BUGTRI slice and the unrodded slice may be compared. The unrodded slice used SCANAL-depleted atom densities, yet both BUGTRI problems show the same BUGTRI-SCANAL reactivity difference. This indicates that the SCANAL nuclide depletion is in reasonable agreement with the BUGTRI depletion despite the differences in the boron absorption fraction. Also, a SCANAL-BUGTRI comparison of the regionwise end-of-cycle nuclide concentrations was made for a number of regions. The SCANAL nuclide concentrations agreed within 2% with BUGTRI.

To explain the cause of the higher SCANAL absorption fraction, various parameters were compared for a radial traverse across the core. It was found that SCANAL predicts a lower fast-to-thermal flux ratio than BUGTRI, with the difference increasing as the reflectors are approached. The lumped burnable poison absorption rate shows the same behavior. It thus appears that the calculated thermal flux in SCANAL is too high, which causes an increase in the lumped burnable poison thermal absorptions. This should also be true for the rod absorptions. Most of these discrepancies are the result of using only one thermal-neutron energy group, which does not allow a correct representation of the spatial change of the neutron energy spectrum and, thus, spatial changes of the effective thermal cross sections.

The above discussion was concerned with a comparison of SCANAL versus BUGTRI-RZ and SCANAL slice versus BUGTRI slice. However, it should be mentioned that the average core GAUGE calculations agree very well with SCANAL and BUGTRI in predicting the core excess reactivity. The GAUGE agreement is, of course, also dependent on the selection of cross sections and the axial leakage. Since GAUGE is cheaper and easier to run than SCANAL, however, it may be more desirable to use GAUGE rather than SCANAL to obtain quick estimates.

SCANAL and BUGTRI refueling region peaking factors are compared in Fig. 2.4. The agreement is fairly good, BUGTRI predicting slightly higher power densities in high-power regions and slightly lower power densities in low-power regions.

Effect of Partially Inserted Control Rods on Axial Power Distribution

During normal reactor operation, most rod pairs will be either fully inserted or fully withdrawn. The central control rod pair typically will operate on automatic control with three rod pairs (one rod group) in a partially inserted position. The partially inserted control pairs will be periodically adjusted, as required, to allow the central control pair mainly to operate in the (axial) central section of the core. However, partial insertion of up to seven rod pairs without limitation on the degree of insertion is acceptable and does not perturb the axial power distribution to such an extent that maximum allowable fuel temperatures are exceeded.

Fig. 2.4--Comparison of refueling region peaking factors from SCANAL and BUGTRI; unrodded beginning of cycle

If for any reason orifice flow control will not be sufficient to prevent excessively high gas outlet temperatures, partial insertion of selective control rod pairs may be used to reduce the power density in the affected regions. At present it is considered permissible to have up to 12 rod pairs partially inserted from 0 to 2 ft for power distribution control in addition to unlimited partial insertion of seven control rod pairs for reactivity control.

To support these assumptions, axial power distribution studies were performed to determine the effect of partially inserted control rods on the gross axial power distribution. Figure 2.5 shows the shift in the axial distribution as three control rod groups (seven rod pairs) are 25%, 50%, and 75% inserted. In the unrodded core, about 65% of the power is generated in the top half of the core and 35% in the bottom half. With a 25% insertion of seven rod pairs, the distribution flattens out to a 59%-41% power split; for a 50% insertion, a 54%-46% power split is obtained. At 75% insertion, the power distribution starts tilting back toward the coolant inlet end (61% top, 39% bottom). In no case does the power distribution tilt toward the bottom, the coolant outlet end of the core.

The effect of additional insertion of 12 rod pairs (i.e., if seven rod pairs are already 50% inserted) is shown in Fig. 2.6. The additional rods in the top 1 and 2 ft of the core tilt the power more strongly toward the outlet end. But even in the worst case, with 12 rod pairs inserted 2 ft, 48% of the power is still generated in the top half of its core. Detailed temperature calculations have shown that this situation is acceptable and does not lead to excessively high fuel temperatures. As a result it is concluded that up to 12 rod pairs may be used for power distribution control as long as they are inserted less than 2 ft.

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.

Shutdown Margin with Two or More Rod Pairs Removed

In order to develop operating procedures and technical specifications, feasible reactivity control limits have to be established including the definition of the minimum number of control rod pairs that must be operable in order to ensure safe shutdown. In the Final Safety Analysis Report, it was shown that at various times in life the reactor can be safely shut down to refueling temperature with any two control rod pairs removed. Minimum shutdown margins occur if two adjacent rod pairs in the outer regions of the core are withdrawn.

Fig. 2.5--Axial power distribution with seven partially inserted control rod pairs

Fig. 2.6--Axial power distribution with seven control rod pairs inserted 50% and an additional twelve inserted 1 and 2 ft

Further studies have been performed on the initial core to investigate the reactivity worth of control rod pairs withdrawn in addition to the two maximum-worth pairs, and the results are presented in Table 2.8. The removal of a third control pair that is at least three regions from the other unrodded regions adds only a negligible amount of reactivity to the reactor. A strong flux tilt is created by the withdrawal of two adjacent rod pairs, and the third rod pair, being removed from a low-flux region, has only a very small effect on the over-all core reactivity.

References

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Table 2.8
 CONTROL ROD WORTHS
 (Initial core, 300°K)

Case	Rod Pairs Withdrawn	k_{eff}	Comment
1	All, unrodded	1.1219	--
2	None, fully rodded	0.8956	Bank = 0.2263 Δk
3	30, 31 (max. 2 rod pairs)	0.9962	2 pairs = 0.1006 Δk
4	30, 31, and 33	0.9978	Third pair = 0.0016 Δk
5	30, 31, and 34	0.9964	Third pair = 0.0002 Δk
6	30, 31, and 12	0.9962	Third pair = 0.0000 Δk
7	30, 31, and 19	0.9962	Third pair = 0.0000 Δk
8	30, 31, and 11	0.9962	Third pair = 0.0000 Δk
9	30, 31, and 37	0.9962	Third pair = 0.0000 Δk

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 reference design fuel for the Fort St. Vrain Nuclear Generating Station has been established as TRISO particles in bonded rods. In October, 1969, blended bed fuel was identified as the reference design fuel element, with the bonded rod as a backup alternative. Fuel tests and manufacturing scaleup activities were carried forward on both blended bed and bonded rod fuel to permit a decision on the Fort St. Vrain fuel to be made following an analysis of irradiation capsule F-27.

The F-27 capsule tested predominantly more advanced bonded rod materials than had previously been tested under irradiation conditions. The bonded rods showed exceptionally good performance. On the basis of these results, together with the good progress in scaling up the bonding process for fuel production, the bonded rod has been chosen for the Fort St. Vrain plant.

Design Changes. Because of the anticipated bowing of fuel elements at the periphery of the core, it was necessary to enlarge the diameter of the top 3.75 in. of the handling hole from 1.375 to 1.75 in. This was done in order to permit free insertion of the fuel handling grapple head into the element handling hole to a sufficient depth to pick up the bowed elements. This change affects 324 of the 1482 elements in the core. Drawings were prepared to describe the new block designs. This brings the total number of block types to seven.

The fuel hole plug design was reevaluated with particular consideration given to assembly problems. It was anticipated that before curing, the plug would slip below the designated position either by its own weight or by the operator who installs the plug and could not be retrieved without drilling. This was confirmed during assembly of six fuel elements for use in fuel handling operations. A new plug was designed and tested with 0.062 in. of one end enlarged enough to ensure a light pressure fit with the fuel hole. The test samples were cemented with type P-511 cement to duplicate the actual interaction between the hole and plug during insertion. Test results of this design indicated that at the maximum interference, the graphite around a fuel hole would not be damaged during installation. Based on the test results, the design was changed to the new configuration.

Blended bed/fuel block interaction. Prior to the termination of the work on blended beds, analyses were made of the interaction between the H-327 graphite fuel block and blended fuel beds. During irradiation, both longitudinal and transverse contraction of the graphite fuel block is predicted up to a maximum of 3% and 1%, respectively, in the two directions. Originally, it was thought that the graphite contraction would cause the loose blended fuel bed to redistribute itself in the fuel hole by an upward flow mechanism, filling a void space provided for that purpose. It was later determined that such fuel particle flow would not occur because of the high aspect ratio (fuel hole length/fuel hole diameter = 60/1). As a consequence, any stress relief accompanying the fuel particle flow would likewise not occur.

This knowledge indicated the possibility that unacceptably high stresses could develop in the graphite fuel element due to blended fuel bed/graphite fuel element interactive dimensional changes. Analytical studies were performed to calculate stress histories in the fuel bed/graphite system. Experimental studies were performed to provide material property data on blended beds needed for the analysis. These were fuel bed modulus and Poisson's ratio in the elastic range for a variety of fuel blends. A triaxial testing apparatus was utilized to determine fuel bed modulus as a function of the hydrostatic state of stress in the blend. Blended bed load-deformation histories were also measured.

Several time-dependent creep analyses were performed using the finite-element computer program SAFE-GRAFITE. Two geometrical models were used to simulate behavior at two different locations in the fuel element. A generalized plane strain model approximated the behavior at a transverse, mid-length plane. In-plane stresses were accurately calculated by accounting for the transverse geometry and transverse temperature distribution between fuel hole and coolant hole. An axisymmetric model considered a longitudinal plane and thus accurately included the effects of longitudinal geometry and temperatures in calculating the axial stresses.

The blended fuel bed was assumed to be a nonlinear, granular, elastic material. Two blends were considered: a stiff fuel particle blend and a soft buffer-overcoated particle blend. Material properties were based on triaxial test data, and TRISO particle data were used for irradiation-induced dimensional changes. Thermal expansion was not considered.

The graphite fuel element was assumed to be a linear viscoelastic material possessing the ability to creep and stress relax with time. Design data were used for thermal expansion and material properties, and design contraction contours were used for irradiation-induced dimensional changes.

Calculations were performed for a 6-year operating period, which corresponds to a maximum fast fluence of 8×10^{21} n/cm².

The strain histories used were calculated by the computer program FESIC. On the basis of the available data, it was assumed that no interactive straining between fuel bed and graphite occurred until after a fast fluence of about 3×10^{21} n/cm². Graphite stresses up to this point are due only to differential internal dimensional changes. After 3×10^{21} n/cm², a fuel bed expansion/graphite contraction mismatch leads to a buildup of stresses in the fuel element.

Representative stress histories for typical high-stress locations are shown in Figs. 3.1 and 3.2. The most significant results are:

1. Transverse stresses are highest away from the ends of the fuel element. Axial stresses are highest at the bottom portion of the fuel element.
2. Graphite stresses increase with increasing fuel bed stiffness.
3. At a reactor shutdown, the maximum additional stress component is about 500 psi.
4. Possible conservatism in the calculations due to uncertainty in the fuel bed dimensional change behavior contributes to the prediction of the high graphite stresses after about four years of reactor operation.
5. The predictability of blended fuel bed/graphite fuel element interactive straining and resulting stresses is subject to some uncertainty. However, the basic behavior has been established by the calculations.
6. Local maximum graphite fuel element stresses after about four years of core irradiation (fast fluence of about $5 \text{ to } 6 \times 10^{21} \text{ n/cm}^2$) may exceed the graphite strength.

Thermal and Coolant Flow Design

Crossflow Correlations. Work has been completed to allow the simple core performance code POKE to include the effects of crossflow previously treated only by the more complex FLAC code. The calculation done by POKE is based on a correlation obtained by using the flow code FLAC. The correlating equation is

$$F_{cf} = C F_o F_p F_a ,$$

where C = a constant for each axial reflector or fuel block,

F_o = a function of orifice setting,

F_p = a function of radial power factor, and

F_a = a function of region age.

These functions have been fitted with mathematical expressions for inclusion in POKE. The crossflow factor is the fraction of outlet flow occurring in any given axial block.

Outlet temperature deviation ($T - \bar{T}$) from the core mixed mean are compared for FLAC and POKE in columns eight and nine of Table 3.1. Comparisons are good except in regions with fully closed orifices where the temperatures are considerably below the mixed mean outlet temperature. Columns two and 11 compare the original FLAC assumptions for orifice settings with the POKE value recalculated to give equal outlet temperature in as many regions as possible. The values here were not expected to agree precisely, since the original FLAC values were merely estimated values based on a prior computation. However, the values do show very similar trends. Comparison of columns 12 and 14 show the effects of crossflow on outlet gas temperature in regions where

Fig. 3.1--Axial stress history in the graphite at location A

Fig. 3.2--Hoop stress history in the graphite at location B

Table 3.1

DATA COMPARISONS OF FLAC AND POKE CORE PERFORMANCE CALCULATIONS

Region No.	Orifice Coeff	Radial Power Factor	Region Age (years)	Inlet CFF ^a (FLAC)	Inlet CFF ^a (POKE)	Ratio Cols 5 to 6	T-TBAR (FLAC)	T-TBAR (POKE)	T-TBAR (POKE) No CF	Results of POKE Recalculation of Orifice Settings			
										Orifice Coeff with CF	T-TBAR with CF	Orifice Coeff No CF	T-TBAR No CF
1	2	3	4	5	6	7	8	9	10	11	12	13	14
1	300.00	0.325	1	---	0.585	---	-263.1	-340.8	-244.2	300.00	-354.8	300.00	-206.4
2	41.72	1.083	3	1.053	0.971	1.084	35.6	-6.3	-36.6	60.42	43.0	39.40	10.9
3	49.75	1.120	4	0.855	0.937	0.912	35.3	59.4	39.5	53.18	43.0	35.79	10.9
4	72.53	1.036	5	0.775	0.856	0.905	34.8	78.9	97.2	71.03	43.0	44.58	10.9
5	300.00	0.765	6	0.518	0.593	0.873	14.5	189.6	422.4	199.14	43.0	95.72	10.9
6	300.00	0.685	1	0.513	0.594	0.863	-57.6	87.1	299.6	284.12	43.0	123.64	10.9
7	69.72	0.974	2	0.901	0.862	1.045	39.6	18.0	32.8	89.44	43.0	52.60	10.9
8	79.41	0.956	5	0.862	0.833	1.034	39.8	31.7	60.6	95.46	43.0	55.23	10.9
9	97.51	0.919	2	0.833	0.796	1.046	41.5	44.9	99.5	110.14	43.0	61.14	10.9
10	300.00	0.741	6	0.534	0.592	0.902	-2.9	159.9	385.5	220.09	43.0	103.15	10.9
11	14.40	1.425	3	1.170	1.150	1.017	38.6	4.0	-73.9	148.40	43.0	16.01	10.9
12	91.41	0.987	1	0.725	0.718	1.010	31.7	90.9	138.5	84.94	43.0	50.80	10.9
13	40.12	1.185	4	0.909	0.975	0.932	33.5	60.1	21.5	42.81	43.0	30.25	10.9
14	36.38	1.189	2	0.909	1.002	0.907	28.1	41.9	-2.8	42.13	43.0	29.93	10.9
15	38.96	1.200	5	0.893	0.982	0.909	28.7	65.7	23.3	40.56	43.0	29.09	10.9
16	12.50	1.479	3	1.163	1.197	0.971	40.8	9.7	-71.3	17.49	43.0	13.74	10.9
17	191.21	0.855	6	0.588	0.671	0.876	26.4	159.5	314.6	138.84	43.0	73.24	10.9
18	42.91	1.080	4	1.040	0.965	1.078	37.8	-2.7	-31.1	61.07	43.0	39.72	10.9
19	164.86	0.821	1	0.709	0.694	1.021	45.7	79.2	205.5	160.84	43.0	80.86	10.9
20	300.00	0.445	1	0.735	0.743	0.989	-131.2	-309.1	-246.2	300.00	-324.3	300.00	-203.8
21	300.00	0.500	6	0.633	0.551	1.149	-154.3	-186.4	17.1	300.00	-205.8	247.61	10.9
22	300.00	0.516	4	0.645	0.559	1.154	-125.6	-166.9	41.4	300.00	-187.0	231.38	10.9
23	300.00	0.490	2	0.704	0.742	0.948	-88.6	-267.0	-196.0	300.00	-283.8	300.00	-149.2
24	300.00	0.770	1	0.543	0.599	0.9065	26.8	128.7	430.1	230.00	43.0	94.26	10.9
25	12.08	1.466	5	1.176	1.319	0.8915	27.4	41.6	-83.3	14.39	43.0	14.27	10.9
26	17.51	1.103	3	1.190	1.329	0.895	33.5	-210.7	-280.4	93.94	43.0	71.36	10.9
27	37.08	1.190	2	0.943	1.077	0.875	36.2	81.5	3.0	35.98	43.0	29.86	10.9
28	300.00	0.535	6	0.546	0.554	0.985	-158.2	-147.9	70.4	300.00	-168.7	213.98	10.9
29	71.85	0.857	4	0.926	1.053	0.879	41.0	-169.8	-209.4	265.63	43.0	140.70	10.9
30	5.15	1.714	3	1.205	1.486	0.811	21.5	78.7	-57.9	6.29	43.0	6.29	10.9
31	80.00	1.069	1	0.730	0.893	0.817	50.4	142.5	161.1	57.27	43.0	40.88	10.9
32	74.20	0.851	5	0.917	1.035	0.885	42.6	-171.1	-207.0	276.27	43.0	143.18	10.9
33	23.81	1.301	4	1.040	1.162	0.895	28.8	69.1	-39.4	24.57	43.0	22.37	10.9
34	242.28	0.750	2	0.602	0.626	0.962	31.5	57.1	290.5	260.17	43.0	100.28	10.9
35	300.00	0.423	6	0.735	0.718	1.024	-157.0	-335.3	-270.7	300.00	-349.5	300.00	-230.5
36	61.01	1.021	5	0.935	0.929	1.006	46.9	39.2	28.1	73.045	43.0	47.27	10.9
37	16.50	1.349	3	1.190	1.272	1.015	30.6	26.9	-88.3	20.88	43.0	19.69	10.9

^aCrossflow factor.

the valve is not fully closed. Crossflow causes an increase in outlet gas temperature of about 32°F in these regions. An additional film drop increase due to reduced flow in the lower portion of the core of about 11°F yields a total fuel temperature increase of 43°F at the core exit.

Field Test Specifications. Field test specifications for low-power radial peaking factor determination and for orifice-outlet temperature calibration have been issued for review. Minimum flow limits have been specified in order to keep fuel temperatures conservatively low while orifice valves are wide open. With the anticipated available flow rates, the maximum fuel temperature in a 1.83 power factor region with no tilt but with hot-spot factors applied is held below 2000°F up to about 16% reactor power. In addition, a correlation has been supplied to allow field computation of the peaking factor from flow and temperature data.

Fission-product Transport

Assembly, welding, and remachining of the test probe has been completed. The basic mechanical concepts of the probe presented no major difficulties, and functional probes should be readily obtainable.

A reverse torque test was run to override the probe locking mechanism, and it was found that about 50 in.-lb (1/6 of the seating torque) was required to fail the latch. As expected, failure occurred by exceeding the yield strength of the spring material in the column buckling mode. While the reverse torque resistance appears more than adequate for the application, it would be advantageous to increase this value to prevent accidental latch damage during installation. The thickness of the spring material will therefore be doubled.

Work on the flow control system for the diffusion tubes is continuing, with emphasis on the continued reliability of the flow in an environment containing fine graphite dust. A satisfactory method for brazing the porous metal tips into the tube ends has been developed and should be repeatable. Tests indicate that a prefilter of Fiberfrax can be used to prevent dust from reaching the flow control tip. However, the density of the filter and its capacity for dust retention are proving to be critical factors in maintaining uniform flow control. Consequently, the size of the diffusion tubes were increased from 1/8 in. OD to 3/16 in. OD to provide more space for the prefilter.

In order to develop a method of testing construction probes for sealability before installation, the test probe was torqued into the test well at increasing torque increments of about 50 in.-lb and the leak rate measured. Between each increase in seating torque the probe was removed and the main O-rings examined. It was found that at about 210 in.-lb the O-rings began to deform to the well seat taper and had achieved a polished contact area on the ring circumference. At this point the main seal leakage had decreased to an easily measureable value, and the thrust screw seal had become leaktight. Thus, both a visual and operational check on the probe seal was obtained. The first seating at this torque produced a tighter seal than the second and subsequent (total of 4) reseatings at the same torque; the latter three being about 1.5×10^{-2} std cm³/sec. For this reason, the best preinstallation test procedure would seem to be (1) to install the probe in the test well at 210 in.-lb and check the leak rate, (2) to remove the probe, checking that a release torque

value of about 100 in.-lb is obtained, and examine the O-rings for indication of full circle contact, and (3) to reinstall the probe at 210 in.-lb and re-measure the leak rate. This procedure is based on results from only one sample probe, but the testing program to date seems to indicate that if a significant sealing effect is observed under these conditions and the O-rings appear as described, a satisfactory seal will result when the full recommended seating torque of 320 in.-lb is applied.

Following the development of the preinstallation test procedure, the test probe was torqued to the design value and a series of ambient temperature seal leak rate tests were conducted. The probe was driven to within about 1/32 in. of its fully seated position when the design torque of 320 in.-lb was applied. The release torque from this point is about 150 in.-lb. Several removals and reseating tests were run and it was found that the leak rate on the main seal in all cases was less than 2.0×10^{-3} std cm³/sec at the operating pressure difference. The thrust seal remained leaktight in each case. Examination of the main O-rings showed good forming to the taper of the well seat with no sign of undesirable distortion.

The next test in the program was a short-time seal test at operating temperature followed by examination of the probe components. To this end the autoclave was prepared, the probe was installed, and the seals subjected to approximately the design pressure differences. Prior to applying heat to the test it was determined that the thrust seal showed no leakage in 18 hr while the main O-ring seals had a leak rate of 7.9×10^{-4} std cm³/sec. This is of the order of 10^{-4} of the normal probe flow rate.

The seal temperature was cycled between ambient and 250°F, and ambient and 500°F. In each case the leak rate decreased as the temperature was increased and did not return to the ambient rate upon returning to ambient temperature. Following these preliminary tests the seals were brought up to the anticipated operating temperature of 750°F and held there for 168 hr. The thrust seal remained leaktight throughout the test, and the main seal leakage decreased to 3.3×10^{-5} std cm³/sec.

At the end of seven days, the temperature was decreased to 500°F and then to 250°F with no increase in leakage of either seal system. At some point below 200°F the main seal leakage began to increase noticeably, reaching 1.4×10^{-2} std cm³/sec at a temperature of 150°F. The system was reheated, and, upon reaching the refueling temperature of 250°F, the leak rate had decreased to 4.0×10^{-4} std cm³/sec. The temperature was then reduced to ambient and the leak rate on the main seal increased to 1.35×10^{-2} std cm³/sec.

Removal of the probe from the well required a torque of 200 in.-lb. This occurred at two distinct points; the first is thought to be the release of the thrust train and the second the release of the main seals. Rotation of the probe to align the flanges for removal from the well required 70 in.-lb of torque. No difficulty was experienced in withdrawal of the probe.

An examination of the test components revealed no mechanical problems, although the typical blue coloring of the stainless steel indicates that the probe had experienced higher temperatures in some regions than were

indicated by the control thermocouples. The silver-plated split-ring seal was in the region of these higher temperatures but showed no adverse effects from the heat. It was noted that where the O-ring silver plating had moved and thickened during the seating of the rings, there were small silver deposits remaining in the well contact area after the probe was removed. A reseating test following probe removal resulted in a lower leak rate than prior to probe removal, indicating that this residue does not interfere with the sealing of the O-rings. It was also found that the material was easily removed by a slight rubbing action.

Conclusions currently drawn from this test are:

1. The probe seals are not adversely affected by temperatures to, and perhaps above, normal expected operating levels.
2. Thermal cycling between refueling and normal operating temperatures will not result in increased seal leakage.
3. In the event of a return to ambient temperature, an acceptable seal is maintained even at full operating pressure difference, and upon reheating, the seal will return to its original effectiveness.
4. Mechanical aspects of the probe are not adversely affected by normal operating temperatures.
5. Any residual silver on the walls of the probe well is soft and easily removed, and apparently does not interfere with the probe seals.
6. The difference noted in the ambient temperature leak rate before and after the test may have been due to O-ring distortion caused by the nonuniform temperature distribution established in the test apparatus.

Work has been initiated to modify the test probe to determine the internal temperature profile of the probe at reactor operating temperatures. This information is needed in order to correctly interpret the plateout profiles in the diffusion tubes. The probe will be fitted with new O-rings, appropriate internals, and a full-length centrally located thermowell to trace the temperature profile within the probe. The autoclave will be mounted on a pivot stand enabling the test to be run in the normal horizontal position. Additional heaters will be installed to create reactor temperatures of 1450°F on the probe barrel and 750°F at the head and seal region. A second thermowell will be installed on the outside of the well to permit tracing the temperature profile in this region also. This installation will also be used for the long-term environmental testing.

The drawings of the plateout probe and well plug have been reworked to reflect changes and corrections and are currently being checked for release for construction. Some aspects of the internal details are being held up pending completion of the temperature profile test and additional studies on the flow control tips for the diffusion tubes.

MATERIALS

The objectives of this subtask are to:

1. Determine the characteristics of beds of 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 including 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 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.
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 fuel elements by means of a series of (1) short-term tests on coated fuel particles to select a coated fuel particle (or particles) having over its design life a high degree of structural integrity 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.

Particle Fuel Systems

Blended Fuel Beds. Prior to the decision to adopt the bonded rod as the reference fuel design, effort was applied to the evaluation of blended fuel beds. This work has now been terminated. The initial work on blended beds was discussed in an earlier quarterly report, GA-9720.

The multiorifice blend technique that has been successfully used to load molds for fuel rods was used with equal success to blend coated particles into graphite fuel channels. Various blend compositions to be used in the core loading were used in this study (see Table 3.2). The blended beds were fabricated by simultaneously metering preweighed amounts of particles through carefully adjusted rectangular orifices. The orifices were adjusted so all of the hoppers emptied at the same time. The filler material used for these studies was activated coconut shell charcoal. The filler material was metered into the particle stream by means of a Syntron vibratory-feeder for all but the larger sizes ($>60\text{ }\mu\text{m}$). This experiment was designed to show the variation in packing fractions as a function of filler size and loading density. Moreover, if any of the various size combinations listed in Table 3.2 were to result in loading difficulties, this would be uncovered by these studies. The conclusions drawn from this work were:

1. The use of larger filler size does not appreciably reduce the packing fraction over that obtained using smaller filler size at the same filler loading.
2. The use of three or more particle sizes results in higher packing fractions than that obtained with two particle size blends.
3. Filler material that is less than $30\text{ }\mu\text{m}$ average size consistently gives low particle packing fractions.

These data suggest that there is no advantage to using a very fine filler particle size, and the more free-flowing larger size is adequate. There is no large penalty in fuel packing involved if the filler material is less than or equal to a loading of about 0.15 g/cm^3 of fuel hole (typical results are presented in Fig. 3.3).

The uniformity of fuel loading is determined by filling a fuel column with a typical particle blend, and then determining the composition of individual segments in the column. The apparatus consists of a 30-in.-long graphite tube that has specially prepared pour spouts at every inch of length. A screening technique performed on the particles of the 30 one-inch segments is an indication of the uniformity of loading. The data presented in Fig. 3.4 indicate that all variations along the length of the column lie within $\pm 4\%$ of the average value. This compares favorably with a design criterion of $\pm 10\%$ of the average value.

The presence of a free surface at the top of each blended bed fuel column would result in some segregation during shipment to the reactor site. In order to eliminate this possibility, a number of restraining methods were investigated. The most promising method is the use of a temporary top plug of polystyrene that decomposes during reactor startup. Shipment tests of such blended beds with the restraining plug followed by analysis of individual 1-in. segments indicate that no segregation occurs.

The thermal conductivity of the blended bed system has been measured a number of times under a variety of conditions. Variables include the specific particle blend chosen, the amount of coke filler, and the packing fraction. Values range from a low of 1.1 to a high of 2.0 in units of $\text{Btu/hr-ft-}^\circ\text{F}$. Recent data are shown in Table 3.3.

Table 3.2

BLENDED BED COMPOSITIONS TYPICAL OF THE FORT ST. VRAIN REACTOR

Blend Composition	Volume Percent ^a			
	Fissile A	Fissile B	Fertile A	Fertile B
1	30	10	0	60
5	20	15	60	5
2	40	0	40	20
7	10	50	0	40
13	10	20	0	70
8	40	20	0	40
(b)	0	50	0	50

^aFor this experiment, the particles are defined as: fissile A, -35 +80 mesh; fissile B, -28 +35 mesh; fertile A, -20 +28 mesh; and fertile B, -14 +20 mesh.

^bThis blend was included for comparison because much previous work was done using this composition, including all of the bonded rod work.

Table 3.3

THERMAL CONDUCTIVITY OF BLENDED BEDS

Test No.	Coke Addition (g/cm ³)	Packing Fraction	Number of Components	Thermal Conductivity (Btu/hr-ft-°F)
H-0367	0.15	0.65	3	2.0
H-0368	0.15	0.56	3	1.6
H-0392	0.15	0.65	3	2.0
H-0402	None	0.58	3 ^a	1.1

^aThe fertile-A component had an additional 40- μ m layer of porous buffer on the outer surface.

Fig. 3.3--Packing characteristics of blend No. 1

Fig. 3.4--Uniformity of particle distribution in a blended fuel bed

The relative coefficient of thermal expansion between a blended bed and a graphite container has been measured to be $2.2 \times 10^{-6} \text{ } ^\circ\text{C}^{-1}$. This is the mean value from room temperature to 1000°C . This value is for unirradiated material, and is a negligible amount of 0.060 in. expansion for a 30-in. fuel column operating at 1000°C .

Additional changes are expected from the irradiation-induced behavior of the blended bed and the graphite. This is illustrated in Fig 3.5, where it is seen that after an exposure to $8 \times 10^{21} \text{ n/cm}^2$, the blended bed length has decreased by 3%, the graphite element length has decreased 3%, and the blended bed diameter has decreased 1%.

The shrinkage of the graphite fuel element is a direct function of the fast-neutron exposure; the shrinkage of the blended bed is more complex. It is shown in Fig. 3.6 that there is an initial shrinkage of the blended bed, out to an exposure of 3 or $4 \times 10^{21} \text{ n/cm}^2$, followed by an expansion, due to the expansion of the isotropic carbon coating layers at this exposure. This results in a net decrease of 3% (see Fig. 3.6) for the blended bed at full exposure ($8 \times 10^{21} \text{ n/cm}^2$).

The blended fuel bed would reside in a cylindrical fuel channel with a length-to-diameter ratio of 60:1. The fuel channel is expected to shrink 3% axially and 1% radially during fast-neutron exposure in the reactor. There are three primary mechanisms whereby the fuel bed can accommodate these shrinkages:

1. The bed can act as a solid body and remain rigid. In this case, the particles will become embedded in the wall as the graphite shrinks.
2. The entire volume shrinkage can be accommodated locally by the fuel particles and coke particles. Particle size distribution and bed packing fraction play an important part in this mechanism.
3. Under certain conditions, the bed can dilate, and allow shear flow to occur. In this case the bed of particles acts as a fluid and rises as the fuel channel shrinks in diameter and length.

The first mechanism was discarded as a design, and efforts were made to utilize either of the other two. One method of utilizing local accommodation of shrinkage is to put a thin skin of weak, porous carbon around some of the particles prior to loading them into the element. During irradiation-shrinkage of the fuel element, the particles will move closer to one another as the strain is accommodated by the soft, porous layers. A second method of utilizing local accommodation of shrinkage is to include in the blended bed a material that will reduce the bed stiffness. The following materials were evaluated in this regard:

1. A slender central tube of graphite that would be crushed locally as the blended bed shrinks.
2. Soft or hollow graphite particles that would be crushed as the blended bed shrinks.

Fig. 3.5 --Predicted dimensional changes in fuel elements containing blended beds of TRISO coated particles in an H-327 graphite block; 1000°C maximum at maximum exposure conditions

Fig. 3.6 --Irradiation-induced volume changes for a blended bed of TRISO coated particles and an H-327 graphite fuel hole; 1000°C maximum

The dilation of the blended bed can occur if the length-to-diameter ratio is reduced from its present value of 60:1 to a value on the order of 6:1. This can be accomplished in a variety of ways.

The experimental work by which these various techniques were evaluated included thermal cycling tests, shrinkage tests of graphite cylinders on blended beds, and stress-strain property values determined with a triaxial test apparatus.

Thermal cycling tests of both BISO and TRISO particles in graphite cylinders have indicated no damage to particles or to the graphite under a variety of conditions up to 130 cycles and 1250°C. Table 3.4 includes a more detailed description of the completed tests.

An additional evaluation test is to utilize the expansion mismatch of copper shot particles and graphite, and simulate the irradiation-induced strain of graphite shrinkage. In these tests, beds of copper shot were thermally cycled in graphite tubes (1/2 in. ID, 1/16 in. wall, 24 in. long) to amplify the strain mismatch. A bed pressure of 200 psi causes tube failure. Table 3.5 indicates that the addition of frangible particles or grafoil wads greatly increases the number of thermal cycles that a blended bed can withstand without failure.

Thin-walled (5/16 in.) graphite tubes filled with a blended bed 0.5 in. diameter by 24 in. long were shrunk to simulate the irradiation-induced shrinkage. Shrinkages of up to 3% (which is what is expected in the reactor core) can be obtained at temperatures up to 1500°C. A bed pressure of 170 to 260 psi causes failure of the tube. Various blended bed compositions have been evaluated, and a sampling of the results is presented in Table 3.6. Shrinkages of up to 3% have been successfully accommodated.

In another study of the graphite-blended bed interaction, ion-exchange resin beads, which are known to expand when exposed to water vapor, were used to simulate a bed of fuel particles in a graphite tube. After exposure to 100% humidity for 26 days, an estimated particle expansion of 25% was obtained, but very little axial or diametral expansion was observed. This suggested that the expansion of the beads was relieved by plastic deformation, and microscopic examination of a cross section of the center of the bed verified that deformation had occurred. It was thus concluded that a column of ion exchange resin beads could not adequately simulate the action of a blended fuel bed.

Various testing procedures were used to determine the stress-strain properties of blended bed geometries. Shear stress-strain curves were generated for various degrees of normal force; yield strengths and wall friction values were also determined. These data were utilized in analyses to predict the behavior of blended beds under various conditions of temperature and stress.

Sorption of Strontium on Carbon Materials. Mass spectrometric experiments have been performed on a series of carbon (coke-type) materials to determine their strontium sorptive capacities. The materials tested include (1) candidates for use as filler material in blended fuel beds and (2) a preparation

Table 3.4

THERMAL CYCLE TESTS OF BLENDED BEDS OF COATED PARTICLES IN H-327 GRAPHITE

Bed Height ^a	Coated Particles	Coke	Temp Range (°C)	Total Cycles ^b	Bed Compaction (%)	Coating Failure (%)
2 in.	TRISO	No	240-840	100	0.3	0
	TRISO	No	240-1250	81	0.3	0
	TRISO	Yes	240-1250	100	0.6	0
	BISO	No	240-840	100	0.2	0
	BISO	No	240-1250	100	1	0
	BISO	Yes	240-1250	130	1	0
10 in.	TRISO	No	240-1250	100	0.4	0
	TRISO	Yes	240-1250	50	0.12	0
	TRISO	Yes	240-1250	49	0.01	0
	TRISO ^c	Yes	240-1250	50	0.52	0
30 in.	TRISO	Yes	RT-1000	50	0.14	0

^aBed diameter 0.5 in.^bTotal cycles predicted for operating HTGR during fuel lifetime is about 50.^cPOCO graphite tube to amplify ratcheting (higher CTE).

Table 3.5
COPPER SHOT THERMAL CYCLE TESTS

Material Added to Bed	Thermal Cycles	Results
None	4,5	Tubes cracked
Frangible particles	23	No failure
Grafoil wads	32	No failure
	50	Tube cracked

Table 3.6
SHRINKING GRAPHITE TUBE TESTS

Material Added to Blended Bed	Shrinkage of Tube (%)	Results
Coke	1,2,3	Tubes cracked
Buffer overcoat	1,2	No failure
Frangible particles	2	Tube cracked

simulating the bonding matrix material of bonded fuel rods. Earlier results were presented in the previous quarterly report, GA-9875.

The samples were loaded with strontium by mixing the carbon material with SrC_2 and heating the mixture for 3 hr at 1350°C in a sealed tantalum container. During the vapor pressure measurements, the samples were contained in a molybdenum Knudsen cell mounted in a mass spectrometer. The temperature range of the investigation was generally between about 1500° and 1800°K . Oxygen contamination of the samples was considered to be insignificant.

The materials studied are specified in Table 3.7. The heat treatments mentioned were performed after receipt of the material and are therefore in addition to those that were an inherent part of the manufacturing process. The heat treatments were performed to remove volatile impurities, in particular hydrogen, from the carbon material. Table 3.7 includes strontium sorptivity values, and the data have purposely been ordered to bring out the apparent correlation between sulfur content and strontium sorptivity. As mentioned in the previous quarterly report, correlation can be rationalized at least on a qualitative basis, from the relative thermodynamic parameters of the S-Sr-C system.

All the candidate carbon materials, except for the bonding material, were tested in the particle size range intended for reactor use. There is not expected to be a large size effect, however, on the basis that loose and bonded charcoals showed only small differences in their sorptive capacity for cesium (see, for example, Table 3.21 in earlier quarterly report GA-7314).

The 1500°K isotherm data are compared in Fig. 3.7. The reference isotherm for 37- to $74\text{-}\mu\text{m}$ grain H-327 graphite is included in Fig. 3.7. The isotherm for bulk graphite is offset from this isotherm toward lower loadings (to the left) by a factor of four. (Bulk graphite, compared to powdered graphite, has a lower sorptive capacity for strontium by a factor of about 4.) Note that two bonded matrix samples were tested and that there is little difference in the resulting isotherms.

From Fig. 3.7, it is evident that the carbon materials fall into two groups distinguished by the slopes of the isotherms. The only correlation that seems to fit is with the sulfur content. One group consists of furfuryl alcohol coke, RH-102 low-strength carbon, and MI-6736 activated charcoal, all with relatively low sulfur content. The isotherms for these materials show comparable sorptive capacities and similar slopes (coincidentally, about the same slope as for graphite). The other group consists of the remaining carbon materials, all of which have relatively high sulfur content. The isotherms for the materials in this group are much steeper.

It is reasonable that the bonded rod matrix material belongs to the second group if one assumes that the pitch residue fraction has a vast majority of the adsorption sites, which is reasonable, and that essentially all the sulfur and strontium are associated with it. On this basis the pitch residue fraction has a sulfur content of 1300 ppm and a strontium sorptivity of 5 mg Sr/g C, and in these characteristics it is close to the calcined petroleum coke containing 4000 ppm sulfur.

Table 3.7
DESCRIPTION OF MATERIALS TESTED AND STRONTIUM SORPTIVE CAPACITY

Material	Heat Treatment			Sulfur Content (ppm)	Particle Size (μm)	Sr ^a Sorptivity (mg Sr/g C)
	Temp (°C)	Time (hr)	Atmos.			
H-327 graphite	--	--	--	<1	37-74	0.04
H-393 purified petroleum coke	1500	1	He	<9	62-125	0.08
Furfuryl alcohol coke	1350	4	Vacuum	<2	62-125	3
RH-102 low-strength carbon ^b	1500	1	He	6	150-250	4
MI-6736 activated charcoal	1350	2	Vacuum	800	62-125	2
Matrix for bonded beds ^c	1500	1	He	700	<1000	3
(Duplicate sample)	1500	1	He	700	<1000	3
Calcined petroleum coke	2000	1	He	4000	62-125	6
Calcined petroleum coke	1500	10	He ^d	9000	62-125	25
Calcined petroleum coke	1500	1	He	9000	62-125	25

^aFor 1500°K and a strontium partial pressure of 10^{-9} atm, which is near the low end of the experimental pressure range but which is higher than expected pressures of strontium nuclides in the reactor.

^bPrepared from 50% phenolic resin, 50% carbon black. Mixed to paste with ethyl cellulose and coked at 700°C.

^cPrepared from 30% 6353 natural flake graphite, 70% coal tar pitch; carbonized at 1000°C; heat-treated for 1/2 hr at 1800°C in He. Approximately 55 wt-% of the final product is the pitch residue. The samples were quite nonuniform in size.

^dHeat treated in fluidized bed.

Fig. 3.7 --Strontium sorption isotherms at 1500°K for several carbons and H-327 graphite

The calcined petroleum coke containing about 9000 ppm sulfur, compared to that containing about 4000 ppm, had a much greater strontium sorptive capacity, indicating sulfur to be especially effective at high concentration in increasing strontium sorptivity.

Two isotherms for the H-393 purified petroleum coke are not included in Fig. 3.7 because of large scatter in the data. The strontium sorptive capacity was clearly less for this coke material than for the other carbon materials, and, in fact, its sorptive capacity more nearly resembled the H-327 graphite (see Table 3.7). This coke material showed a well-developed (X-ray) crystallinity that was not observed for the calcined petroleum coke.

All the materials so far tested, except the H-393 petroleum coke, have strontium sorptive capacities 100 times or more greater than reference bulk graphite at the lower pressure limit (approximately 10^{-9} atm) of the experiment.

One must conclude that the sorption isotherms for various carbon materials can be quite different, and there does not seem to be a ready explanation for these differences. The intersecting of isotherms for two different materials is perplexing. The only apparent correlation is with sulfur content, but this is not a simple one. At the lower pressure limit of the experimental range, where reactor conditions are more nearly approximated, there is a direct correlation between sulfur content and strontium sorptive capacity. Other important parameters almost certainly exist.

Although sulfur has an apparent beneficial effect on sorptivity, the possible release of sulfur from the coke material into the helium coolant could have deleterious effects on metallic components of the reactor system. A substantial loss of sulfur from the coke material would also affect its strontium sorptive capacity. Accordingly, the vaporization of sulfur from carbon materials is being investigated.

A sample of calcined petroleum coke was placed in a molybdenum Knudsen cell, which was mounted in a mass spectrometer. Vapor pressures of the sulfur-containing gaseous species were observed in the temperature range 1600° to 1800°K as the sulfur content decreased from 8000 ppm to 4000 ppm. (These concentrations are somewhat uncertain because of analytical difficulties.)

The results given in Table 3.8 show that the vapor pressures of the sulfur-containing species are quite small and depend on the sulfur concentration. The vaporization rates have a strong temperature dependence as shown by the large heats of vaporization. These heats were measured only at the higher concentrations and may not be applicable at the lower concentrations.

Sulfur-containing vapor species were observed in experiments in which a sample of calcined petroleum coke had previously been impregnated with strontium. Although the sulfur concentrations were not well known, it appears that the partial pressures of the sulfur species were not drastically altered, even though the strontium loading was about 30 mg Sr/g C. A small amount of SrS(g) was observed in the experiments.

Table 3.8

VAPOR SPECIES AND PRESSURES OVER CALCINED PETROLEUM COKE AT 1800°K

Gaseous Species	Partial Press. at S Concentration of 8000 ppm (atm)	Partial Press. at S Concentration of 4000 ppm (atm)	ΔH (kcal/mole)
S	3.6×10^{-7}	2.6×10^{-8}	122
S ₂	3.9×10^{-7}	3.3×10^{-9}	118
CS	8.7×10^{-8}	4.9×10^{-9}	129
CS ₂	2.0×10^{-7}	5.5×10^{-9}	134
SiS	1.3×10^{-7}	6.8×10^{-9}	87

An experiment designed to roughly approximate reactor geometry conditions was performed. A sample of calcined petroleum coke material was added to a molybdenum cell, which was mounted in the mass spectrometer. The cell was covered with a tight-fitting H-327 graphite cover 0.5 cm thick. The cover had no orifice or openings, except for channels inherent in the graphite. The weight of the coke material was about 0.01 that of the graphite lid, approximately the weight ratio of coke to graphite in the reactor.

On heating, there appeared to be no time lag for transport of sulfur through the graphite lid at 1500°K. At 1800°K, the only vapor species observed to vaporize through the lid were small amounts of CS(g) and SiS(g). The experiment is being analyzed.

Impurity Content of Fuel Element Materials. One additional sample of coke material was examined for hydrogen content during this report period. This new coke material is a commercially purified petroleum coke designated H-393. The sample was outgassed at 1000°C and exposed to water vapor (at a relative humidity of 62%) at 85°C. The resultant water pickup after 22 days exposure was immeasurably low. It is concluded that use of this coke material would add a negligible amount of hydrogen to the as-loaded core.

In addition, the hydrogen content of plastic materials that were considered for possible use as plugs in fuel element channels was measured. These materials were 10% polystyrene in MEK and 10% collodion in ether. Results show that up to 8.5 lb of hydrogen might be added to the as-loaded core if plastic plugs made from these materials were used to secure loose fuel particles.

Graphite Development

The fuel element and reflector graphite material is designated grade H-327 and is made from a calcined petroleum coke (needle type) filler with a maximum particle size not exceeding 0.06 in. Coal tar pitch is used as the binder and may also be used as the impregnant if density increases are required. These two constituents of H-327 graphite and the oils used during their

extrusion into log form are from a controlled source of supply. Graphitization of the extruded logs is carried out in batch furnaces at the specified temperature of 2800°C (+300°C, -100°C). Each log weighs approximately 500 lb and bears an identification number throughout fabrication and machining to its final arrangement as a nuclear fuel containment. At the end of the quarter, 885 logs of H-327 grade graphite had been extruded, graphitized, and accepted by Gulf General Atomic. All are acceptable as Grade A or B materials according to the specified gradings based on nuclear purity and mechanical strength.

Mechanical Properties. The extruded logs of graphite produced for machining into fuel blocks are 18 in. in diameter and 34 in. long. The largest differences in mechanical properties of the log are between the center and outer cross-section positions at the end and mid-length planes in the extrusion direction. Approximately 1% of the logs produced for the fueled zone of the core have been mechanically tested by tensile testing two or three samples machined from the transverse- and parallel-to-extrusion orientation at each of the above log locations. Table 3.9 lists the tensile properties of the most recently tested graphite selected at approximately the mid-stage of production. The largest spread in strength and modulus of elasticity values within an H-327 graphite log is across the mid-length section. For example, the ultimate tensile strength in the parallel orientation is 70% greater at the edge of the log than in the center, and in the transverse direction, the edge section is 30% stronger. The mechanical anisotropy (parallel value/transverse value) in a fuel block graphite is a minimum of 1.65 with a maximum for the ultimate tensile strength of 2.1, and a minimum of 2.1 with a maximum of 3.4 for the chord modulus of elasticity measured between stress values of 250 and 500 psi (see Table 3.10). The relative ductility of the two principal directions in the fuel block graphite can be assessed from the strain at fracture values. The graphite has a maximum strain at fracture across the fuel block direction in the range of 0.2% to 0.4%, which is 20% to 50% greater than the strain at fracture along the length of the block.

Fuel Block Web Integrity. A contract has been placed with Southwest Research Institute, San Antonio, to study the feasibility of inspection of graphite webs by eddy currents. The program will be phased to optimize eddy-current equipment design, to evaluate optimized design efficiency in testing fuel blocks, and to build prototype equipment for production inspection.

Currently, an eddy-current probe design based upon a dual-encircling-coil type of system has been built and tested, and found suitable for flaw detection in graphite. While a variety of frequencies are probably usable, studies on samples characterized by a considerable variation of grain structures indicate the most useful frequency is 100 kcps. This value is based on flaw detectability and signal-to-noise ratio. At present, a 45-mil-diameter through hole is considered the smallest detectable hole in a fine structured graphite. In this type of material, which is "clean" enough that a 40-mil through hole can be detected, a 60-mil-diameter hole penetrating 50% to 75% of the ligament and having a blind end at the bore of the fuel hole is detectable with a signal-to-noise ratio of approximately 2:1. Further work is required to study the feasibility of detecting defects that do not have sharp profiles. The feasibility of detecting laminated discontinuities adjacent to fuel block holes has been demonstrated, and sectioning of the defect areas showed that signals similar to that given by a 100-mil hole penetrating 75%

Table 3.9

TENSILE PROPERTIES OF GRADE A H-327 GRAPHITE LOG NO. 42/1972 85/0069

Property	Sample Position							
	Parallel Orientation				Transverse Orientation			
	End Edge	Mid-length Edge	Mid-length Center ^a	End Center	End Edge	Mid-length Edge	Mid-length Center	End Center
Ultimate tensile strength, psi:								
Max	2340	2160	1480	2540	1460	1150	950	1100
Min	2250	2130	1340	2525	1420	1060	890	1080
Mean	2293	2145	1410 ^a	2532	1440	1105	920	1090
Cumulative mean	2361	2410	1469	1968	1397	1155	889	1327
Current std dev.	218	355	415	462	232	223	160	338
Strain at fracture, 10 ⁻³ in./in.:								
Max	2.30	1.90	1.60	2.20	3.80	2.58	2.63	2.50
Min	1.70	1.80	1.10	2.10	3.20	2.13	2.40	2.48
Mean	1.90	1.85	1.35	2.15	3.50	2.35	2.52	2.49
Cumulative mean	2.24	1.90	1.69	1.8	3.39	2.33	2.025	2.49
Current std dev.	0.412	0.5367	0.5106	0.4208	0.9189	0.5458	0.4396	0.8054
Secant modulus, 10 ⁶ psi:								
Max	1.38	1.20	1.20	1.19	0.77	0.50	0.40	0.44
Min	1.00	1.12	0.92	1.14	0.37	0.45	0.34	0.44
Mean	1.21	1.16	1.06	1.16	0.57	0.47	0.37	0.44
Cumulative mean	1.31	1.20	1.23	1.02	0.56	0.57	0.37	0.467
Current std dev.	0.3446	0.1690	0.1720	0.2516	0.1788	0.1396	0.1145	0.095
Chord modulus between 250 and 500 psi, 10 ⁶ psi:								
Max	1.9	2.50	1.6	1.60	0.62	0.55	0.46	0.50
Min	1.25	1.60	1.0	1.50	0.45	0.50	0.41	0.60
Mean	1.58	2.05	1.3	1.55	0.53	0.52	0.43	0.55
Cumulative mean	1.38	1.65	1.76	1.35	0.66	0.549	0.519	0.59
Current std dev.	0.53	0.5965	0.167	0.1245	0.2339	0.1404	0.066	0.039

^aVendor's test result 1400 psi.

Table 3.10
MECHANICAL ANISOTROPY OF H-327 GRAPHITE
(Parallel Orientation/Transverse Orientation)

Property	Log Location			
	End Edge	Mid-length Edge	Mid-length Center	End Center
Ultimate tensile strength	1.69	2.08	1.65	1.56
Strain at fracture	0.66	0.815	0.83	0.723
Secant modulus	2.34	2.1	3.31	2.18
Chord modulus	2.09	3.0	3.4	2.29

of the web thickness were given by lamina-type discontinuities with major dimensions of 90 by 150 mils.

Sections of graphite containing 100- to 140-mil lamina-type defects have been machined with a fuel hole adjacent to the defect. This size of defect was the largest discontinuity found by radiography of a whole 18-in.-diameter by 34-in.-long log. One out of four such defects has been found to be detectable against the high background noise of H-327 graphite. Since most of the defects were located many defect-diameters away from the bore of the hole, it is thought that the noise level of the graphite may help to identify defects in significant locations only.

Control Materials

Three additional experiments on the behavior of boronated graphite materials at 2950°C were performed for the purpose of measuring (1) diffusion coefficients for boron transport and (2) slumping behavior of control rod material. The temperature of 2950°C is calculated to occur in the center of the core during the hypothetical loss-of-forced-cooling (LOFC) accident.

In two of the experiments, diffusion coefficients for boron through H-327 graphite were determined using the constant-source method. This method yields diffusion profiles represented by the equation

$$\frac{C}{C_0} = \operatorname{erfc} [x/2 (Dt)^{1/2}] ,$$

where C/C_0 = boron concentration in graphite relative to the concentration at the source interface, and

x = distance in graphite, cm.

This equation is specific for slab geometry and is utilized in the present experiments by determining the concentration profile through the bottom or side wall of the graphite crucible that contained the specimen. Inverse error functions of the concentration data (C/C_0) are determined using standard tables and plotted versus distance (x), and D is calculated from the slope of the profile.

The third experiment was a standard high-temperature slumping test performed on a sample of production control material supplied by the manufacturer. Slumping tests were described in the previous quarterly report, GA-9875. They involve subjecting the specimen to a compressive load of 16 psi (the maximum load on the boronated compacts in the center of the core as calculated to occur during the LOFC accident due to melting of metal components and subsequent slumping of the control rod). The specimen was annealed at 2950°C for ten hours. Boron loss, degree of sample slumping, and effective diffusion coefficient for boron transport from the compact material were determined.

The slumping test using the production sample was performed to evaluate the acceptability of a particular batch of control material which presumably contained more boron than the specification allowed. The boron density specification for the central control rod compacts is 0.48 +0.04, -0.02 g B/cm³. The manufacturer's analysis of this particular batch was 0.56 g B/cm³.

The results of the three tests are given in Table 3.11. Error function diffusion profiles for experiments 4705-66 and 4705-72 are shown in Fig. 3.8. The straight lines on this type of plot attest to the validity of the model. It is of interest that the concentration profile measured radially through the side wall of the crucible for Exp. 4705-72 yields a diffusion coefficient that is about one-half of that obtained from the profile measured through the bottom (see Fig. 3.8). This difference is expected because the mathematical model is exact for planar geometry, not cylindrical. In addition, a small amount of gravity flow could contribute to an increased diffusion coefficient through the bottom of the crucible.

Effective diffusion coefficients for boron losses from the test specimens were calculated using the relationship:

$$F = \sqrt{Dt} \, 2.256/L \quad ,$$

where F = fraction boron lost,

D = effective diffusion coefficient, cm²/sec,

t = time, sec, and

L = radius of cylinder, cm.

The diffusion coefficients thus calculated (see Table 3.11) are lower than those calculated using the constant-source method.

Analysis at Gulf General Atomic of this batch of material showed that the boron density was 0.52 g/cm³, which is clearly within the specification.

Table 3.11
RESULTS OF ANNEALS AT 2950°C ON BORONATED GRAPHITE MATERIALS

Exp. No.	Initial Boron Content ^a (g B/cm ³)	Time (hr)	Boron Trans- port (%)	Sample Compression ^b (%)	Diffusion Coefficients (cm ² /sec)	
					Constant- Source Method ^c	Boron- Loss Method ^d
4705-70	0.52	10	46.2	4.9	ND	4.5x10 ⁻⁷
4705-66	0.50	6	20.0	ND ^e	2.7x10 ⁻⁶	2.3x10 ⁻⁷
4705-72	0.50	8	20.1	ND	3.1x10 ⁻⁶	1.7x10 ⁻⁷

^aAnalyses performed at Gulf General Atomic.

^bThe 16-psi compressive load was applied during experiment 4705-70 only.

^cDiffusion coefficients in H-327 graphite determined by constant-source (or error-function) method using concentration profiles measured in bottom of crucible.

^dDiffusion coefficients in boronated graphite specimen determined using fractional release model for cylindrical samples (see text).

^eND, not determined.

Fig. 3.8--Boron concentration profiles through H-327 graphite at temperature of $2950^\circ \pm 50^\circ\text{C}$

The degree of slumping for this sample was 4.9% (see Table 3.11), which is consistent with that found on other specimens of similar or lower boron content (see the previous quarterly report, GA-9875). These results show that the material is acceptable.

The diffusion coefficients for boron transport through H-327 graphite as measured by the constant-source method of experiments 4705-66 and -72 (see Table 3.11) are consistent with the value of 3×10^{-6} cm²/sec (at 2950°C) used in safety analyses. Effective diffusion coefficients, calculated on the basis of boron loss, are significantly lower than the value used in safety analyses, indicating that the boron redistribution calculations for the LOFC accident based on diffusion through the graphite are conservative.

Irradiation Testing

Irradiation experiments are being conducted to proof-test TRISO and BISO coated particles, fuel rods, and blended beds under Fort St. Vrain design irradiation exposures. These experiments are intended to confirm the selection of the reference fuel designs and to demonstrate the performance of these materials under Fort St. Vrain service conditions.

During this report period, postirradiation examination of two high-exposure tests of TRISO particles (P22 and F-27) has been conducted and a third capsule (F-28) completed irradiation to half Fort St. Vrain exposure. One half-exposure (F-25) and two full-exposure (F-26 and F-29) are under irradiation.

Capsule P22. Irradiation of capsule P22 was completed during the quarter. The capsule received a peak fast-neutron fluence of 7.7×10^{21} n/cm² ($E > 0.18$ Mev), equivalent to full Fort St. Vrain exposure. Postirradiation examination is currently in progress.

The capsule contained samples of early production-type TRISO particles with pyrolytic carbon coatings deposited from propane-acetylene mixtures. These samples are not representative of current reference production material. The coating variables tested in P22 were designed to evaluate the effects of PyC type, PyC density, and SiC thickness on stability at high exposures. Also included were laboratory-prepared TRISO and advanced-design SiC-coated fissile particles, and TRISO coated fertile particles.

Preliminary results are given in Table 3.12. These results show that LTI coatings perform better than HTI coatings, confirming what has been observed in other recent experiments (e.g., capsule P20). Additionally, for the mixed gas LTI coatings, the density should be restricted below about 1.90 g/cm³ to ensure satisfactory irradiation performance. The metallographic examination of the P22 samples will be made following the completion of the postirradiation examination of capsule F-27.

Capsule F-25. Capsule F-25 is a half-exposure (4×10^{21} n/cm²) proof test in the ETR of nine TRISO coated particle and 15 fuel rod samples. All coated particle and most fuel rod samples were made in production equipment. This capsule is a backup proof test (to F-28) to control fuel specification for the first half of the core.

Table 3.12

DESCRIPTION OF COATED PARTICLES TESTED IN CAPSULE P22
(8-cycle ETR irradiation)

Lot No.	Type ^a	Coating										Particle ^c	Irradiation Conditions				Postirradiation Examination				Remarks				
		Buffer		Isotropic		SiC		Isotropic		Total Coating Thickness (μm)	Type		Size (μm)	Th:U Ratio	Temperature (°C)			Fast Fluence (10 ²¹ n/cm ²)	Macro Exam (% Fail)			Met Exam (% Fail)			
		Thick (μm)	Density (g/cm ³)	Thick (μm)	Density (g/cm ³)	Thick (μm)	Density (g/cm ³)	Thick (μm)	Density (g/cm ³)						B.A.F. ^b	Burnup ^d (% FIMA)	Avg		Max	EOL ^e		PyC	SiC	PyC	SiC
4155-141E ^f	TRISO-I LTI	50	1.26	—	—	21	3.17	38	1.79	1.05	109	(Th,U)C ₂	150-250	2.75	20	1090	1245	1010	6.6	0.2	0.1	—	—	TRISO-I LTI variable density series	
3923-63E	TRISO-I LTI	54	1.03	—	—	20	(g)	46	1.90	1.02	120	(Th,U)C ₂	150-250	2.75	20	1095	1340	930	7.7	0	—	—	—		
4155-143E ^f	TRISO-I LTI	49	1.26	—	—	21	3.17	46	1.96	1.04	116	(Th,U)C ₂	150-250	2.75	20	1085	1235	1000	6.6	70	2.6	—	—		
4033-87E	TRISO-I HTI	49	1.10	—	—	21	3.19	52	1.94	1.00	122	(Th,U)C ₂	150-250	2.73	20	1090	1245	1010	6.6	0.2	—	—	—	TRISO-I HTI	
4155-149E ^f	TRISO-II LTI	55	1.10	20	(g)	20	3.15	38	1.80	1.03	133	(Th,U)C ₂	150-250	2.75	20	820	915	785	7.7	2.5	0.1	—	—	TRISO-II LTI variable density and SiC thickness series	
4155-151E ^f	TRISO-II LTI	49	1.22	20	(g)	19	3.15	29	1.88	1.05	117	(Th,U)C ₂	150-250	2.75	20	1095	1295	900	7.7	0.1	—	—	—		
4155-139E ^f	TRISO-II LTI	49	1.22	20	(g)	18	3.19	36	1.95	1.03	123	(Th,U)C ₂	150-250	2.75	20	1045	1295	900	7.7	62	0.2	—	—		
4155-135E ^f	TRISO-II LTI	57	1.17	19	(g)	15	3.14	35	1.92	1.07	126	(Th,U)C ₂	150-250	2.75	20	990	1095	1020	7.3	52	1.7	—	—		
4155-131E ^f	TRISO-II LTI	46	1.23	18	(g)	24	3.15	33	1.87	1.07	121	(Th,U)C ₂	150-250	2.79	20	715	915	845	7.3	3.3	0.5 ^g	—	—		
4155-145E ^f	TRISO-II HTI	49	1.22	20	(g)	18	3.18	32	1.85	1.00	119	(Th,U)C ₂	150-250	2.75	20	620	915	465	7.7	88	<0.1	—	—	TRISO-II HTI variable density and SiC thickness series	
4033-31	TRISO-II HTI	51	1.08	20	1.95	20	3.19	28	1.88	1.01	119	(Th,U)C ₂	150-250	2.73	20	1015	1095	1050	7.3	98	0.6	—	—		
4155-147E ^f	TRISO-II HTI	49	1.22	20	(g)	18	3.18	33	1.93	1.03	120	(Th,U)C ₂	150-250	2.75	20	980	1275	785	7.7	96	0.3	—	—		
4155-133E ^f	TRISO-II HTI	57	1.17	19	(g)	14	3.11	32	1.89	1.04	122	(Th,U)C ₂	150-250	2.75	20	820	915	785	6.6	74	0.7	—	—		
4155-137E ^f	TRISO-II HTI	46	1.23	18	(g)	24	3.13	31	1.90	1.03	119	(Th,U)C ₂	150-250	2.79	20	1155	1365	1100	7.3	96	0.2	—	—		
4043-105	TRISO-II HTI	55	1.40	21	1.85	18	3.17	29	1.90	0.98	123	ThC ₂	415-495	—	7	1185	1365	1100	7.3	6	0	—	—	Fertile series	
4042-129	TRISO-II LTI	55	1.40	21	1.85	20	3.19	34	1.92	1.04	130	ThC ₂	415-495	—	7	555	780	435	7.3	1.4	0.9 ^h	—	—		
4033-75E	Adv. Design ⁱ	46	1.06	19	2.03	20	3.22	60 ^j	1.62	1.01	145	(Th,U)C ₂	150-250	2.73	20	1110	1325	1050	6.6	<0.1	—	—	—	Experimental design series	
4042-95E	TRISO-II HTI (Si doped)	51	1.08	20	1.95	20	3.19	35	2.05	1.03	126	(Th,U)C ₂	150-250	2.73	20	1085	1255	1010	6.6	0.1	—	—	—		
3923-63E	TRISO-I LTI	54	1.03	—	—	20	(g)	46	1.90	1.02	120	(Th,U)C ₂	150-250	2.73	20	1030	1150	955	5.2	0	—	—	—	1100°C variable isotropic density series	
4155-149E ^f	TRISO-II LTI	55	1.10	20	(g)	20	3.15	38	1.80	1.03	133	(Th,U)C ₂	150-250	2.75	20	895	1005	850	5.2	0.2	—	—	—		
4155-151E ^f	TRISO-II LTI	49	1.22	20	(g)	19	3.15	29	1.88	1.05	117	(Th,U)C ₂	150-250	2.75	20	950	1055	900	5.2	0.1	—	—	—		
4155-139E ^f	TRISO-II LTI	49	1.22	20	(g)	18	3.19	36	1.95	1.03	123	(Th,U)C ₂	150-250	2.75	20	950	1055	900	5.2	3	0	—	—		
4155-145E ^f	TRISO-II HTI	49	1.22	20	(g)	18	3.18	32	1.85	1.00	119	(Th,U)C ₂	150-250	2.75	20	970	1075	920	5.2	1.2	0.2	—	—		
4155-147E ^f	TRISO-II HTI	49	1.22	20	(g)	18	3.18	33	1.93	1.03	120	(Th,U)C ₂	150-250	2.75	20	895	1010	850	5.2	2.4	0	—	—		
3923-63E	TRISO-I LTI	54	1.03	—	—	20	(g)	46	1.90	1.02	120	(Th,U)C ₂	150-250	2.73	20	915	965	915	6.0	0	—	—	—	900°C variable isotropic density series	
4155-149E ^f	TRISO-II LTI	55	1.10	20	(g)	20	3.15	38	1.80	1.03	133	(Th,U)C ₂	150-250	2.75	20	920	920	920	6.0	0.2	0.1	—	—		
4155-151E ^f	TRISO-II LTI	49	1.22	20	(g)	19	3.15	29	1.88	1.05	117	(Th,U)C ₂	150-250	2.75	20	920	920	920	6.0	0.2	—	—	—		
4155-139E ^f	TRISO-II LTI	49	1.22	20	(g)	18	3.19	36	1.95	1.03	123	(Th,U)C ₂	150-250	2.75	20	920	955	920	6.0	1.8	0	—	—		
4155-145E ^f	TRISO-II HTI	49	1.22	20	(g)	18	3.18	32	1.85	1.00	119	(Th,U)C ₂	150-250	2.75	20	920	920	920	6.0	0.8	0	—	—		
4155-147E ^f	TRISO-II HTI	49	1.22	20	(g)	18	3.18	33	1.93	1.03	120	(Th,U)C ₂	150-250	2.75	20	800	955	920	6.0	3.7	0	—	—		

^fTRISO-I denotes a coating with a coating sequence of buffer, SiC, and LTI or HTI isotropic PyC. TRISO-II denotes a coating sequence of buffer, LTI isotropic PyC, SiC and LTI or HTI isotropic PyC. LTI denotes low-temperature isotropic outer pyrolytic carbon coating, and HTI denotes high-temperature outer isotropic pyrolytic carbon coating.

^gMacro anisotropy factor.

^hAll particles are spherical and >90% of theoretical density.

ⁱEstimated for eight cycles in the ETR.

^jEnd-of-life temperature.

^kDenotes particles made in production equipment; other particles are made in laboratory equipment.

^lNot determined.

^mThermal reactions observed.

ⁿOuter coating consists of 25 μm buffer on SiC, with final coating of 35 μm HTI pyrolytic carbon.

The F-25 capsule is in its fourth cycle in the ETR in the F-13 core position. The capsule is operating satisfactorily, with most fuel rod samples close to the design temperature of 1125°C (programmed downward from 1250°C). Both coated particle thimble sample cells are operating below design temperature (1125°C) with the fissile samples about 50°C low and the fertile samples about 150°C low. The fuel has achieved an estimated fast fluence of 4.5×10^{21} n/cm² to date, with an estimated burnup of 17% FIMA. Design exposures are 4×10^{21} n/cm² and 18% FIMA, respectively. The capsule will complete its scheduled irradiation at the end of the current cycle.

Capsule F-26. Capsule F-26 is a full-exposure (8×10^{21} n/cm²) proof test in the ETR of eleven fuel rod, four blended bed, and nine TRISO coated particle samples. All coated particle and most fuel rod samples were made in production equipment. This capsule is a backup proof test (to capsule F-29) to control fuel specification for the second half of the core.

The F-26 capsule is in its third cycle in the I-13 core position of the ETR. The capsule is operating very satisfactorily with all monitored fuel rod, blended bed, and coated particle thimble samples close to the design temperature of 1250°C and the majority of the thermocouples still operative. To date the capsule has received an estimated fast fluence of 3.3×10^{21} n/cm², with an estimated fuel burnup of 15% FIMA. Design exposures are 8×10^{21} n/cm² and 23% FIMA, respectively. The capsule is expected to complete irradiation in early 1971.

Capsule F-27. Capsule F-27, the first test of reference production samples, completed its scheduled four-cycle irradiation in the GETR during the quarter. The capsule received a peak fast fluence of 6.0×10^{21} n/cm² ($E > 0.18$ Mev), which is equivalent to four years of peak exposure in the core. (This is 50% higher than the anticipated exposure of 4×10^{21} n/cm²; consequently, capsules F-28 and F-29 will complete their design exposures ahead of schedule.)

The fuel samples tested in capsule F-27 are summarized in Table 3.13. A detailed description of all coated particle samples in the capsule is given in Table 3.14. Included were nine fuel rod, nine blended bed, and nine TRISO coated particle samples. The coated particle (and most fuel rod) samples were made in production equipment and are expected to be representative of materials selected for the core. Four prototype Peach Bottom Core 2 fuel compact samples were also included.

The principal fuel rod variables tested were binder (pitch and Si), filler (natural flake graphite and isotropic graphite), process (hot injection, hot pressing, and SiC sintering), heat treatment (2000°C for 0.5 hr, 1800°C for 0.5 hr, and 1000°C for 1 hr), and coating type (TRISO-LTI deposited from propylene, propane, and mixed gas). All fuel rod samples were 0.5 in. in diameter by 0.75 in. long.

The principal blended bed variables tested were restraint (graphite felt, carbon felt, and temporary binder) and coated particle type (TRISO-LTI deposited from propylene and propane and BISO-LTI deposited from propylene). All samples contain charcoal filler. The blended beds were 0.5 in. in diameter by 0.75 in. long.

Table 3.13

SUMMARY OF FUEL SAMPLES TESTED IN CAPSULE F-27

Capsule Position and Design Temp	Sample Type	Bed/Rod			Coating Type	Coated Particles				
		Binder/ Restraint ^a	Filler ^b	Heat Treat. (°C)		Gas ^c	Iso Layer		Lot No.	
							Fissile	Fertile		
									Fissile	Fertile
Cell 1, 1250°C:										
A	Blen. bed	GF	Charc.	--	BISO	PL	1.87	1.71	4000-189	4000-188
B	Fuel rod	SI	---	1850	TRISO	PL	1.78	1.76	4632-47	4632-41
C	Blen. bed	GF	Charc.	--	TRISO	PL	1.83	1.71	4000-192 ^d	4000-193
D	Blen. bed	GF	Charc.	--	TRISO	PP	1.83	1.75	4000-182	4000-185
E	Blen. bed	CF	Charc.	--	TRISO	PL	1.78	1.74	4000-183	4000-194
Cell 2, 1250°C:										
1	Thimble	--	---	--	BISO	MG	1.88	1.88	4000-174 ^e	4000-173
2	Thimble	--	---	--	BISO	PL	1.87	--	4000-189	---
3	Thimble	--	---	--	Bare SiC	PL	--	--	4000-190	---
4	Thimble	--	---	--	TRISO	PL	1.78	--	4000-183	---
5	Thimble	--	---	--	TRISO	PP	1.83	--	4000-182	---
6	Thimble	--	---	--	TRISO	PP	1.54	--	4000-181	---
Cell 3, 1250°C:										
1	Thimble	--	---	--	TRISO	PL	--	1.74 ^f	---	4000-194
2	Thimble	--	---	--	TRISO	PP	--	1.75 ^g	---	4000-185
3	Thimble	--	---	--	Bare SiC	PL	--	--	---	4000-195
Cell 4, 1250°C:										
A	Blen. bed	GF	Charc.	--	TRISO	PL	1.78	1.74	4000-183	4000-194
B	Fuel rod	Pitch	NF	2000	TRISO	PP	1.81	1.76	4000-177	4000-176
C	Blen. bed	Temp	Charc.	--	TRISO	PL	1.78	1.74	4000-183	4000-194
D	Fuel rod	Pitch	NF	1800	TRISO	PL	1.78	1.74	4000-183	4000-194
E	Fuel rod	Pitch	Iso	2000	TRISO	PP	1.81	1.76	4000-177	4000-176
Cell 5, 1250°C:										
A	Fuel rod	Pitch	NF	1000	TRISO	PP	1.81	1.76	4000-177	4000-176
B	Compact	Pitch	GP-38	1800	BISO	MG	1.88	1.86	4000-174	4000-160
C	Compact	Pitch	GP-38	1800	BISO	MG	1.88	1.86	4000-173	4000-160
D	Compact	Pitch	NF	1800	BISO	MG	1.88	1.86	4000-174	4000-160
E	Fuel rod	Pitch	Iso	1800	TRISO	MG	1.79	1.73	4000-175	4000-178
Cell 6, 800°C:										
A	Fuel rod	Pitch	Iso	2000	TRISO	PP	1.81	1.76	4000-177	4000-176
B	Fuel rod	Pitch	NF	1800	TRISO	PL	1.78	1.74	4000-183	4000-184
C	Blen. bed	GF	Charc.	--	TRISO	PL	1.78	1.74	4000-183	4000-194
D	Compact	Pitch	GP-38	1800	BISO	MG	1.88	1.86	4000-174	4000-160
E	Blen. bed	GF	Charc.	--	TRISO	PP	1.83	1.75	4000-182	4000-185
F	Fuel rod	SI	---	1850	TRISO	PL	1.78	1.76	4632-47	4632-41
G	Blen. bed	GF	Charc.	--	TRISO	PL	1.83	1.71	4000-192 ^d	4000-193

^aGF = graphite felt; CF = carbon felt; temp = temporary binder.^bNF = natural flake graphite; Charc. = charcoal; Iso = isotropic graphite.^cPL = propylene; PP = propane; MG = mixed gas (propane-acetylene). Inner isotropic is deposited from same gas as outer isotropic.^dHigh BAF.^eLow/high Th, a mixture of fissile and fertile particles.^fMeasurement on particle coating gave 1.67 g/cm³.^gMeasurement on particle coating gave 1.86 g/cm³.

Table 3.14

DESCRIPTION OF COATED PARTICLE SAMPLES IN CAPSULE F-27

Lot No.	Type	Coating												Total Coating Thick. (μm)	Kernel			Used In		
		Buffer		Isotropic			SiC		Isotropic				Type		Size (μm)	Th:U Ratio	Blen. Beds	Fuel Rods	Thimble	
		Thick. (μm)	Density (g/cm ³)	Thick. (μm)	Density (g/cm ³)	BAF	Thick. (μm)	Density (g/cm ³)	Thick. (μm)	Density ^f (g/cm ³)	BAF	Dep From ^a								
4000-192	TRISO	50	1.30	22	1.79	1.12	20	3.20	31	1.83	1.08	PL	120	(Th,U)C ₂	105-175	2.75	X			
4000-183	TRISO	50	1.15	20	1.83	1.06	20	3.18	43	1.78	1.04	PL	131	(Th,U)C ₂	175-275	2.75	X	X	X	
4000-181	TRISO	50	1.08	20	1.74	1.06	20	3.22	48	1.54	1.02	PP	135	(Th,U)C ₂	175-275	2.75			X	
4000-177	TRISO	45	1.30	19	1.87	1.02	19	3.22	39	1.81	1.01	PP	120	(Th,U)C ₂	175-275	2.75		X		
4000-182	TRISO	52	1.11	25	1.82	1.05	16	3.17	39	1.83	1.02	PP	128	(Th,U)C ₂	175-275	2.75	X		X	
4000-175	TRISO	43	1.10	18	1.86	1.05	17	3.19	44	1.79	1.03	MG	126	(Th,U)C ₂	175-275	2.75		X		
4000-184	TRISO	56	1.00	32	1.84	1.07	21	3.18	51	1.76	1.05	PL	145	ThC ₂	450-590	--		X		
4000-193	TRISO	49	1.28	17	1.90	1.30	21	3.20	51	1.71	1.14	PL	130	ThC ₂	450-590	--	X			
4000-194	TRISO	55	1.26	24	1.81	1.12	21	3.19	53	1.74 ^d	1.10	PL	145	ThC ₂	450-590	--	X	X	X	
4000-185	TRISO	53	1.20	28	1.88	1.08	20	3.19	58	1.75 ^e	1.04	PP	159	ThC ₂	450-590	--	X		X	
4000-176	TRISO	46	1.26	24	1.82	1.08	20	3.19	55	1.76	1.00	PP	146	ThC ₂	450-590	--		X		
4000-178	TRISO	52	1.19	20	1.81	1.02	19	3.19	54	1.73	1.02	MG	137	ThC ₂	450-590	--		X		
4000-190	Bare SiC	50	1.15	20	1.83	1.06	20	3.18	--	--	--	PL	90	(Th,U)C ₂	175-275	2.75			X	
4000-195	Bare SiC	55	1.26	24	1.81	1.12	22	3.19	--	--	--	PL	101	ThC ₂	450-590	--			X	
4000-189	BISO	63	1.20	--	--	--	--	--	64	1.87	1.07	PL	127	(Th,U)C ₂	175-275	2.75	X		X	
4000-188	BISO	68	1.29	--	--	--	--	--	76	1.71	1.04	PL	144	ThC ₂	450-590	--	X			
4000-174	BISO	54	1.34	--	--	--	--	--	82	1.90	1.02	MG	136	(Th,U)C ₂	350-450	5		X	X	
4000-173	BISO	55	1.31	--	--	--	--	--	58	1.86	1.03	MG	113	(Th,U)C ₂	420-495	18		X	X	
4000-160	BISO	44	1.35	--	--	--	--	--	88	1.86	1.03	MG	132	ThC ₂	350-450	--		X		
4632-47 ^b	TRISO-Si	50	1.15	20	1.83	1.06	20	3.18	43	1.78	1.04	PL	135	(Th,U)C ₂	175-275	2.75		X		
4632-41 ^c	TRISO-Si	56	1.00	32	1.84	1.07	21	3.18	51	1.76	1.05	PL	153	ThC ₂	450-590	--		X		

^aPL = propylene; PP = propane; MG = mixed gas.^bLot 4000-183 overcoated with 4 μm Si/SiC.^cLot 4000-184 overcoated with 8 μm Si/SiC.^dMeasurement on particle coating gave 1.67 g/cm³.^eMeasurement on particle coating gave 1.86 g/cm³.^fMeasured on disk.

The principal coated particle variables tested were coating type (BISO and TRISO), LTI layer type (deposition from propylene and propane for TRISO and from propylene and mixed gas for BISO), and LTI layer density (low and medium for TRISO propane samples). Included also were TRISO samples without an outer isotropic layer (TRISO-III) to evaluate the necessity of this layer, since some samples in previous capsule tests have survived with the SiC layer intact despite failure of the outer isotropic layer. A total of six fissile and three fertile samples were tested.

Postirradiation examination of the F-27 samples is currently in progress. A summary of the first results is given below.

1. The Fort St. Vrain prototype pitch-bonded fuel rods were generally in very good condition (see Table 3.15 and Figs. 3.9 through 3.15). Visual inspection showed the rods with isotropic filler to be in slightly better condition than the rods having natural flake filler. A high-temperature preirradiation anneal ($\geq 1800^{\circ}\text{C}$) of the rods appeared to enhance stability. Two rods (4B and 6B) prepared in production equipment were inadequately injected with matrix material; as a result, they partially debonded during irradiation. One of these rods also contained a fertile particle sample (lot 4000-194), which had significant failure as a thimble sample and in blended beds (see item 2 below).
2. Dimensional change measurements indicated that the fuel rods shrank 1% to 2% in both length and diameter. The data are given in Table 3.15. The rods with isotropic filler had slightly better dimensional stability.
3. Initial metallographic results on the fuel rods showed no evidence of coating failure from matrix-to-coating interactions in any rod (see Fig. 3.16). The matrices were in good condition, with those containing isotropic graphite filler slightly denser than those with natural flake filler (see Fig. 3.17); however, little difference in performance was noted between the two filler types. Composite photomicrographs showing radial cross sections of each fuel rod (at the approximate axial midplane) are shown in Figs. 3.18 through 3.23.
4. The three batches of TRISO fissile particles all survived the irradiation. The two batches of TRISO fertile (Fertile-B) particles had about 10% and 50% failure of both the outer PyC and SiC coatings. Similarly, a TRISO-III (TRISO without the outer PyC) fissile survived whereas a TRISO-III fertile had about 10% failure. The three BISO particle batches survived the irradiation without failure. The results are given in Table 3.16, and photographs of the samples are shown in Figs. 3.24 through 3.32).
5. The five blended beds containing TRISO particles irradiated at about 1250°C all had excessive failure of the fertile particles; these samples (with one exception) also had fissile particles that had greater than 1% failure. The particles in beds irradiated at 800°C appeared to have survived the irradiation. The blended bed results are given in Table 3.17, and photographs of the samples are shown in Figs. 3.33 through 3.39. In bed No. 6C, which was irradiated

Table 3.15

RESULTS OF POSTIRRADIATION EXAMINATION ON FUEL RODS IRRADIATED IN CAPSULE F-27

Rod No.	Type	Matrix			Particles ^b	Irradiation		Postirradiation Examination			Remarks
		Binder	Filler ^a	Heat Treat. (°C)		Temp (°C)	Fast Fluence (10 ²¹ n/cm ²)	Appearance	Dimensional Change (%)		
									Diam.	Length	
6A	Hot injection	Pitch	Iso	2000	TRISO-PP	800	4.1	Large crack	-0.4	-0.3 ^c	Rod slightly cracked as fabricated
5E	Hot injection	Pitch	Iso	1800	TRISO-MG	1250	5.0	Very good	-1.7	-0.8	
4E	Hot injection	Pitch	Iso	2000	TRISO-PP	1250	5.7	Very good	-1.1	-0.9	
6B ^d	Hot injection	Pitch	NF	1800	TRISO-PL	800	3.7	Some loose particles	-1.4 ^c	-1.4 ^c	Portions of rod inadequately injected
4D ^d	Hot injection	Pitch	NF	1800	TRISO-PL	1250	5.7	Debonded; particle failure	---	---	Bad fertile lot
4B	Hot injection	Pitch	NF	2000	TRISO-PP	1250	5.7	Good	-1.4	-1.0 to -1.7	Low-temp heat treat
5A	Hot injection	Pitch	NF	1000	TRISO-PP	1250	5.6	Dark; cracks	-1.6	-2.2	
1B	Diff. bonding	Si-SiC	---	1850	TRISO-PL	1250	4.4	Debonded	---	---	Matrix disintegrated
6F	Diff. bonding	Si-SiC	---	1850	TRISO-PL	800	2.5	Debonded	---	---	Matrix disintegrated

^aIso = isotropic graphite; NF = natural flake graphite.^bAll particles coated in 5-in. production coaters. PP = propane; PL = propylene; MG = mixed gas.^cApproximate measurement.^dRod produced in production equipment; others produced in laboratory equipment.

PREIRRADIATION

POSTIRRADIATION
(800°C, 4.1×10^{21} n/cm²)

Fig. 3.9--Pre- and postirradiation photographs of fuel rod 6A; pitch binder, isotropic graphite filler, heat-treated at 2000°C

PREIRRADIATION

POSTIRRADIATION
(1250°C, 5.0×10^{21} n/cm²)

Fig. 3.10--Pre- and postirradiation photographs of fuel rod 5E; pitch binder, isotropic graphite filler, heat-treated at 1800°C

PREIRRADIATION

POSTIRRADIATION
(1250°C, 5.7×10^{21} n/cm²)

Fig. 3.11--Pre- and postirradiation photographs of fuel rod 4E; pitch binder, isotropic graphite filler, heat-treated at 2000°C

PREIRRADIATION

POSTIRRADIATION
(800°C, 3.7×10^{21} n/cm²)

Fig. 3.12--Pre- and postirradiation photographs of fuel rod 6B; pitch binder, natural flake graphite filler, heat-treated at 1800°C

PREIRRADIATION

POSTIRRADIATION
(1250°C, 5.7×10^{21} n/cm²)

Fig. 3.13--Pre- and postirradiation photographs of fuel rod 4D; pitch binder, natural flake graphite filler, heat-treated at 1800°C

PREIRRADIATION

POSTIRRADIATION
(1250°C, 5.7×10^{21} n/cm²)

Fig. 3.14--Pre- and postirradiation photographs of fuel rod 4B; pitch binder, natural flake graphite filler, heat-treated at 2000°C

PREIRRADIATION

POSTIRRADIATION
(1250°C, 5.6×10^{21} n/cm²)

Fig. 3.15--Pre- and postirradiation photographs of fuel rod 5A; pitch binder, natural flake graphite filler, heat-treated at 1000°C

M340-145

75X

M340-125

75X

Fig. 3.16--Representative photomicrographs showing condition of coated particles and matrix of rod 5E with isotropic graphite filler (above) and rod 4B with natural flake graphite filler (below). Note that upon shrinking, the matrix breaks away from the particle coatings without damaging them

M340-146

250X

M340-134

250X

Fig. 3.17--Representative photomicrographs showing the isotropic graphite filler matrix material of rod 5E (above) and the natural flake graphite filler matrix material of rod 4B (below)

Edge

K-79707

50X

Center

Fig. 3.18--Composite photomicrograph showing radial cross section of fuel rod No. 4B (pitch binder, natural flake graphite filler, 2000°C heat treatment) after irradiation to 5.7×10^{21} n/cm² at 1250°C

Fig. 3.19--Composite photomicrograph showing radial cross section of fuel rod No. 4E (pitch binder, isotropic graphite filler, 2000°C heat treatment) after irradiation to 5.7×10^{21} n/cm² at 1250°C

Fig. 3.20--Composite photomicrograph showing radial cross section of fuel rod No. 5A (pitch binder, natural flake graphite filler, 1000°C heat treatment) after irradiation to 5.6×10^{21} n/cm² at 1250°C

Edge

K-79711

Center

Fig. 3.21--Composite photomicrograph showing radial cross section of fuel rod No. 5E (pitch binder, isotropic graphite filler, 1800°C heat treatment) after irradiation to 5.0×10^{21} n/cm² at 1250°C

Edge

K-79708

Center

Fig. 3.22 --Composite photomicrograph showing radial cross section of fuel rod No. 6A (pitch binder, isotropic graphite filler, 2000°C heat treatment) after irradiation to 4.1×10^{21} n/cm² at 800°C

Edge

K-79709

Center

Fig. 3.23--Composite photomicrograph showing radial cross section of fuel rod No. 6B (pitch binder, natural flake graphite filler, 1800°C heat treatment) after irradiation to 3.7×10^{21} n/cm² at 800°C. (Note inadequate injection of matrix in center of rod)

Table 3.16

RESULTS OF POSTIRRADIATION EXAMINATION ON COATED PARTICLE SAMPLES IRRADIATED IN CAPSULE F-27

Lot No.	Coating ^a			Kernel Type	Irradiation		Postirradiation Examination (% Failure)				Remarks
	Type	Density (g/cm ³)					Macro Exam		Met Exam		
		Disk	Particle ^b		Temp (°C)	Fast Fluence (10 ²¹ n/cm ²)	PyC	SiC	PyC	SiC	
181	TRISO-PP	1.54	1.59	Fissile-B	1250	6.0	0.5	0.1	(c)	(c)	Lot 183 without outer PyC
182	TRISO-PP	1.83	1.88	Fissile-B	1250	6.0	0	0	(c)	(c)	
183	TRISO-PL	1.78	1.84	Fissile-B	1250	6.0	0.9	0	1.8	0	
190	Bare SiC	--	--	Fissile-B	1250	6.0	---	0.1	---	0.4	
189	BISO-PL	1.87	--	Fissile-B	1250	6.0	0	---	(c)	(c)	
185	TRISO-PP	1.75	1.86	Fertile-B	1250	5.7	11	7	14	14	Lot 194 without outer PyC
194	TRISO-PL	1.74	1.67	Fertile-B	1250	5.7	~50	~40	≥25	20	
195	Bare SiC	--	--	Fertile-B	1250	5.7	---	~10	---	~3.5	
174	BISO-MG	1.90	--	Low Th ^d	1250	6.0	0	---	(c)	(c)	
173	BISO-MG	1.86	--	High Th ^d	1250	6.0					

^aAll particles coated in 5-in. production coaters. PP = propane; PL = propylene; MG = mixed gas.

^bDensity determined on outer PyC removed from coated particle.

^cIn progress.

^dPeach Bottom Core 2 prototype fuel.

Fig. 3.24--Photograph of TRISO coated fissile-B particle lot 181 (propane) after irradiation to 6.0×10^{21} n/cm² at 1250°C

Fig. 3.25--Photograph of TRISO coated fissile-B particle lot 182 (propane) after irradiation to 6.0×10^{21} n/cm² at 1250°C

Fig. 3.26--Photograph of TRISO coated fissile-B particle lot 183 (propylene) after irradiation to 6.0×10^{21} n/cm² at 1250°C

Fig. 3.27--Photograph of TRISO-III coated (bare SiC) fissile-B particle lot 190 after irradiation to 6.0×10^{21} n/cm² at 1250°C

Fig. 3.28--Photograph of BISO coated fissile-B particle
lot 189 (propylene) after irradiation to 6.0×10^{21} n/cm²
at 1250°C

Fig. 3.29--Photographs of TRISO coated fertile-B particle lot 185 (propylene) after irradiation to 5.7×10^{21} n/cm² at 1250°C

Fig. 3.30--Photographs of TRISO coated fertile-B particle lot 194 (propylene) after irradiation to 5.7×10^{21} n/cm² at 1250°C

Fig. 3.31--Photographs of TRISO-III coated (bare SiC) fertile-B particle lot 195 after irradiation to 5.7×10^{21} n/cm² at 1250°C

Fig. 3.32--Photograph of BISO (mixed gas) coated low-thorium and high-thorium fissile Peach Bottom Core 2 samples 173 and 174 after irradiation to 6.0×10^{21} n/cm² at 1250°C

Table 3.17

RESULTS OF POSTIRRADIATION EXAMINATION ON BLENDED BEDS IRRADIATED IN CAPSULE F-27

Bed No.	Bed Design		Particles ^b	Irradiation		Postirradiation Examination				Remarks
						% Failure		Length Changes ^c (%)		
						Type	Macro Exam			
	Temp (°C)	Fast Fluence (10 ²¹ n/cm ²)		PyC	SiC					
Filler	Restraint ^a	Temp (°C)	Fast Fluence (10 ²¹ n/cm ²)	Type	PyC	SiC	Length Changes ^c (%)	Remarks		
6G	Charcoal	GF	TRISO-PL	800	2.0	Fiss Fert	0 0	0 0	(d)	Particles stuck into large lump
6E	Charcoal	GF	TRISO-PP	800	2.9	Fiss Fert	(e)	(e)	(d)	
6C	Charcoal	GF	TRISO-PL	800	3.5	Fiss Fert	0 0.1	0 0	(d)	
1A	Charcoal	GF	BISO-PL	1250	3.9	Fiss Fert	0 4.5	-- --	-20.5	
1C	Charcoal	GF	TRISO-PL	1250	4.8	Fiss Fert	3 7	3 4.5	-17.2	High BAF fissile
1E	Charcoal	CF	TRISO-PL	1250	5.2	Fiss Fert	0.1 6.5	0 4	-14.3	Bad fertile lot
4C	Charcoal	Temp	TRISO-PL	1250	5.7	Fiss Fert	2.5 -50	1 -50	-10.9	Bad fertile lot
4A	Charcoal	GF	TRISO-PL	1250	5.7	Fiss Fert	4.5 -50	2 -50	-14.4	Bad fertile lot
1D	Charcoal	GF	TRISO-PP	1250	5.0	Fiss Fert	8.5 7.5	8.5 6.5	-15.6	

^aGF = graphite felt, CF = carbon felt; temp = temporary binder.^bAll particles coated in 5-in. production coaters. PP = propane; PL = propylene.^cDetermined from neutron radiography; diametral changes are currently being determined from measurements of the inside diameters of the crucibles.^dNot determined.^eIn progress.

Fig. 3.33--Photographs of blended bed 6C containing TRISO coated (propylene) particles and charcoal with graphite felt restraint after irradiation to 3.5×10^{21} n/cm² at 800°C

Fig. 3.34--Photographs of blended bed 1A containing BISO coated (propylene) particles and charcoal with graphite felt restraint after irradiation to 3.9×10^{21} n/cm² at 1250°C

Fig. 3.35--Photographs of blended bed LC containing TRISO coated (propylene) particles and charcoal with graphite felt restraint after irradiation to 4.8×10^{21} n/cm² at 1250°C

Fig. 3.36--Photographs of blended bed 1E containing TRISO coated (propylene) particles and charcoal with carbon felt restraint after irradiation to 5.2×10^{21} n/cm² at 1250°C

Fig. 3.37--Photographs of blended bed 4C containing TRISO coated (propylene) particles and charcoal with a temporary binder after irradiation to 5.7×10^{21} n/cm² at 1250°C

Fig. 3.38--Photographs of blended bed 4A containing TRISO coated (propylene) particles and charcoal with graphite felt restraint after irradiation to 5.7×10^{21} n/cm² at 1250°C

Fig. 3.39--Photographs of blended bed LD containing TRISO coated (propane) particles and charcoal with graphite felt restraint after irradiation to 5.0×10^{21} n/cm² at 1250°C

at a low temperature, it was found that some fuel particles in the center had stuck together into a lump (Fig. 3.33). No sticking was observed in any of the other beds.

6. Preliminary dimensional measurements of the blended beds showed significant shrinkages in bed heights (approximately 15% for TRISO beds and 20% for the BISO bed). These data are summarized in Table 3.17.
7. Metallographic examination of blended bed 4C showed amoeba-type movement of the fuel kernels through the particle coatings in about 15% of the fissile particles (Fig. 3.40); the SiC coating was penetrated in about one-third of these particles. No amoeba movement was observed in the fertile particles, although a high percentage were observed to have failure of both PyC coatings; many also had SiC coating failure (Fig. 3.41).
8. The two silicon-bonded fuel rods were debonded and many of the SiC coatings were observed to have failed. Photographs of these samples are shown in Figs. 3.42 and 3.43.

Postirradiation examination of the F-27 capsule is continuing. Final results will be reported next quarter.

Capsule F-28. Capsule F-28 is a proof test of reference production fuel samples to half exposure (4×10^{21} n/cm²). The results from this experiment will control the start of fuel production for the first half of the core. Eight fuel rod, six blended bed, and nine TRISO coated particle thimble samples are being tested. Two of the blended bed samples are 0.4 in. in diameter by 0.4 in. long and contain reference TRISO particles with low- and medium-density coatings.

The F-28 capsule was removed from the GETR after three cycles of irradiation, and is currently in the hot cell awaiting postirradiation examination. The irradiation was terminated one cycle earlier than planned because dosimetry results from the F-27 capsule confirmed that the flux in the GETR is higher than originally anticipated. Capsule F-28 received an estimated peak exposure of 4.5×10^{21} n/cm² in its three cycles.

Capsule F-29. Capsule F-29 is a proof test of reference production fuel samples to full exposure (8×10^{21} n/cm²). The results from this experiment will control the start of fuel production for the second half of the core. The samples being tested in F-29 are identical to those in F-28, except that two sintered SiC matrix rods have been substituted for the two prototype Peach Bottom Core 2 compact samples. The F-29 samples were described in the previous quarterly report, GA-9875.

The F-29 capsule is in its fourth of six cycles in the GETR (E-5 core position) and is operating satisfactorily. The monitored fuel rod, blended bed, and coated particle samples in cells 1 to 5 are operating close to the design temperature of 1125°C. The beds and rods in cell 6 are operating at about 650°C, 250°C below their design temperature of 900°C. The fuel has reached an estimated fast fluence of 5.5×10^{21} n/cm² with an estimated burnup of 17% FIMA (high uranium content samples). Design exposures are 8×10^{21} n/cm² and 23% FIMA (high uranium samples) or 5% FIMA (low uranium samples). The capsule is scheduled to complete irradiation in June 1970.

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Fig. 3.40--Photomicrograph of TRISO fissile particle (lot 4000-183) from blended bed 4C showing amoeba movement of the fuel kernel resulting in penetration of the SiC coating

Fig. 3.41--Photomicrograph of TRISO fertile particle (lot 4000-194) from blended bed 4C showing failure of PyC and SiC coatings

PREIRRADIATION

POSTIRRADIATION
(1250°C, 4.4×10^{21} n/cm²)

Fig. 3.42--Pre- and postirradiation photographs of silicon-bonded fuel rod 1B

PREIRRADIATION

POSTIRRADIATION
(800°C, 2.5×10^{21} n/cm²)

Fig. 3.43--Pre- and postirradiation photographs of silicon-bonded fuel rod 6F

Capsule F-30. The F-30 capsule was to have been an irradiation test of a full-sized (0.5 in. diameter by 30 in. long) blended bed of TRISO particles to full exposure (8×10^{21} n/cm²). Work on this capsule and an associated test to measure vibration levels in the GETR core has been terminated in accordance with the selection of fuel rods as the Fort St. Vrain reference fuel design.

Graphite Irradiations. Fuel capsules F-25 and F-26 described above contain one graphite irradiation cell each designed to operate at temperatures that cover the lower half of the HTGR service temperatures (600° to 800°C). The cells contain H-327 graphites previously irradiated to 3 to 4 x 10²¹ n/cm² (E > 0.18 Mev), samples of production grade H-327, and a number of isotropic graphite grades I and II. The mean operating temperature of the graphite cell in F-25 is 650°C as scheduled, and the graphite cell in F-26 is operating slightly below the design temperature of 750°C (center of cell).

Capsule F-28 contains piggyback samples of two isotropic graphites and production H-327, operating at 400° and 700°C.

CHEMISTRY

The objectives of this subtask are to determine:

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

Fission-product Release

The release of cesium from TRISO coated fuel particles during irradiation in the P22 capsule was determined by gamma spectrometric analyses. The main purpose of this work was to determine the magnitude of the cesium release from coated particles during irradiation to relatively high burnup.

In earlier measurements, TRISO coated particles irradiated to a burnup of about 11% FIMA in the Pl3F capsule exhibited very low in-pile Cs¹³⁷ release values (in the range of 10⁻⁴ to 10⁻⁵). In-pile Cs¹³⁷ release values for TRISO coated particles irradiated in the Pl3H capsule to high burnup (up to

22% FIMA) at temperatures up to 1300°C were in the range of 10^{-4} to 10^{-1} (see earlier quarterly report GA-8725), indicating failure of the SiC coating or some of the particles.*

In the present work, samples consisting of about 15 intact particles were gamma-counted using a Ge(Li) detector in conjunction with a Scientific Data Systems Sigma II 4096-channel analyzer-computer. The computer processed the raw counting data and calculated (using a special code) the fractional release of Cs^{137} during the irradiation. The calculation was based on the relation

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

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 sample per unit reactor power, y_j = the fission yield of nuclide j , λ_j = the decay constant of nuclide j , and t_i = the time at the end of interval i . Values of k were calculated using the activity of Zr^{95} as the reference fission product. Complete retention of the reference fission products is assumed in the calculation.

Table 3.18 gives in-pile Cs^{137} fractional release data determined in preliminary analyses of selected samples of particles irradiated at three temperatures in capsule P22. The release values were all less than 0.05 (the uncertainty of each determination), indicating that the TRISO coating is effective in retaining cesium. The P22 capsule was recently removed from the ETR, and the radiation levels of the particles were too high for accurate analyses. The count rates were so high that good gamma peak resolution and integration were not possible. This probably accounted for the 11% variation in the $\text{Cs}^{137}/\text{Zr}^{95}$ activity ratios.

Impurity Attack on Fuel Materials

Steam-graphite Reaction Rate Studies. Experiments to determine the rate of steam oxidation of H-327 graphite have been performed on specimens taken from a production log (log 2, pilot run 3, No. 83/9976-C-42/Z163-3-1). Specimens were cut perpendicular to and parallel to the direction of log extrusion and were taken from locations near the edge and radial center of the log. A specimen of AGOT graphite was tested along with the H-327 graphite specimens. The specimens used were 1 in. wide, 2 in. long, and 0.125 in. thick.

Graphite specimens were oxidized up to 8% weight loss (burnoff) using 2.8% H_2O in helium gas. Weight loss was measured, as in earlier work, by

*The in-pile release data for the P13F and P13H capsules were determined by measuring the metallic fission products collected in the graphite thimbles and crucibles used to hold the particles. It was assumed that all the metallic fission products that escaped the particles were trapped in the thimbles and crucibles. This assumption may not have been valid for cesium. The procedure used in the other previous measurements is similar to that used in the present measurements.

Table 3.18

FRACTIONAL RELEASE OF Cs^{137} FROM TRISO COATED PARTICLES DURING
IRRADIATION IN THE P22 CAPSULE^a

Lot No. ^b	Cell	Thimble	Coating Type	Design ^c Irrad. Temp (°C)	Ratio of Cs^{137} to Zr^{95} in dpm ^d	Cs^{137} Release Fraction
4155-139E	1	2	TRISO-II LTI	900	0.091	<0.05
4155-145E	1	3	TRISO-II-HTI	900	0.083	<0.05
4155-147E	1	4	TRISO-II-HTI	900	0.091	<0.05
4155-151E	1	5	TRISO-II-LTI	900	0.089	<0.05
4155-149E	1	6	TRISO-II-LTI	900	0.084	<0.05
4155-139E	3	2	TRISO-II-LTI	1300	0.085	<0.05
4155-145E	3	3	TRISO-II-HTI	1300	0.083	<0.05
4155-147E	3	4	TRISO-II-HTI	1300	0.086	<0.05
4155-151E	3	5	TRISO-II-LTI	1300	0.087	<0.05
4155-149E	3	6	TRISO-II-LTI	1300	0.086	<0.05
4155-139E	5	2	TRISO-II-LTI	1100	0.091	<0.05
4155-145E	5	3	TRISO-II-HTI	1100	0.089	<0.05
4155-147E	5	4	TRISO-II-HTI	1100	0.088	<0.05
4155-151E	5	5	TRISO-II-LTI	1100	0.088	<0.05
4155-149E	5	6	TRISO-II-LTI	1100	0.092	<0.05

^aThe time at full power was 252 days.

^bParticle descriptions were taken from Table 3.9 of earlier quarterly report GA-9261. TRISO-II denotes a coating sequence of buffer, LTI isotropic PyC, SiC, and LTI or HTI isotropic PyC. LTI denotes low-temperature outer PyC layer, and HTI denotes high-temperature outer PyC layer.

^cThe temperatures given are average design temperatures and may be somewhat different when final analysis has been completed.

^dCorrected to end of irradiation.

use of an automatic recording electrobalance. The resulting reaction rate data for the H-327 and AGOT graphite samples are plotted versus $1/T$ in Fig. 3.44. The rate data for H-327 graphite appear quite normal and are similar to the reaction rate data for AGOT graphite. No indication of the anomalous behavior observed previously was found.

Fig. 3.44--Temperature dependence of steam-graphite reaction rate data measured at conditions of 2.8% H₂O in helium with a helium flow rate of about 150 cm³/min

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 by testing it with a segment of a dummy core and by testing critical components, as necessary, in helium at the temperature and chemical impurity level expected in the plant.

The fuel handling machine and control system has successfully completed the fuel element handling tests in air, utilizing the automatic control hardware and software. The tests indicate that the total system is operating in accordance with the design goals.

Prior to conducting the in-air refueling tests, a number of changes were made in areas where earlier tests had indicated a need for correction. The major changes involved the vertical drive system, the grapple head rotation system, and the strain gage signal conditioners. Also, minor modifications were made to the refueling data tapes and the computer program.

The closed-loop control of the vertical drive system was optimized with the new drive motors. During development of the final maximum speed profile, it became obvious that the acceleration limiter would not function properly utilizing the differentiated tachometer signal as feedback. To correct this situation, a position error rate-of-change limiter was designed which uses the differentiated position error signal as feedback. This method is successful because the vertical drive velocity is nearly proportional to position error, and there is only a small phase lag in this feedback loop.

New solenoid valves were installed on the grapple head rotation system, and the controller was modified to drive them. Changes were also made in the voltage-to-pressure controller to eliminate an intermittent lockup condition in its circuits. An artificial deadband of $\pm 1^\circ$ of rotation was put in the positioning loop to prevent the rotational transients that occurred during certain conditions of element pickup and deposit.

Just prior to the start of the in-air tests, while attempting to deposit a 30° rotated element, it was discovered that the inner tube guide roller was loose. This caused excessive play in the θ rotation system. Corrective action was taken and, at the same time, the new R centering switches were installed.

Near the completion of the in-air tests, the R-drive system was observed to require higher current readings than normal, and noise could be heard emanating from the R-drive mechanism. The mechanism was disassembled and the ball screw and thrust bearings were found to be rough due to the presence of corrosion products. These items were replaced by on-hand spares which were dry-film lubricated with a resin base rather than a silicate base which is more hygroscopic. The machine was reassembled and returned to service.

Two mounting schemes for the Z-drive yoke position potentiometer were installed and tested. The second of the two schemes was found to be satisfactory.

The signal conditioning units for the strain-gage transducers were returned from the supplier and installed and are performing satisfactorily.

A test specification for the handling of bowed elements was prepared and issued for review.

Design of a mechanically actuated valve to be incorporated into the grapple head to prevent release of an element unless all weight was relieved from the grapple probe was initiated.

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 tube integrity are also planned.

MODULE AIR-FLOW TEST

Installation of strain gages on the steam generator air-flow test module is about 65% complete. The test loop was completed and started up. Alkaline-treated water is being circulated to clean up the loop before connecting the steam generator module. A dry-air purge system is being installed for lay-up of the steam generator between test runs. The data acquisition system was completed, except for calibration of the transducers.

The specification for the air-flow test was issued for review.

MATERIALS TESTS

The creep tests on Incoloy 800 at Gulf General Atomic were completed, and the results are being evaluated. The tests of hold-time effects on the low-cycle strain-controlled fatigue life of Incoloy 800 were started at Battelle Memorial Institute and are continuing.

TUBE PLUGGING MACHINE

Installation drawings for the tube plugging machine are now essentially complete.

The tube plugging machine in the seal-weld configuration was refurbished during this reporting period and final checkout was initiated. A cooling water leak in the welding head has delayed completion of the checkout. The leak resulted in an electrical short circuit in the torch. In addition, water was found to have run down the elevating mast, causing the vertical drive mechanism to be inoperative.

The torch and the defective cooling line were replaced, and disassembly and cleanup of the vertical drive mechanism were initiated late in the quarter.

Fabrication of the tools for the tube plugging configuration was initiated.

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 investigation of fabrication methods and coating process variables and by preirradiation evaluation of coated particles.

COATED PARTICLE EVALUATION

Alpha-counting of bulk particles is being investigated as a means of determining the level of fuel contamination on or near the surface of TRISO coated fuel particles. Levels of fuel contamination can be related to fission-gas release behavior (see earlier quarterly report GA-9130). The goal of this work is to develop the alpha-counting technique as a low-cost method for establishing that the production fuel particles meet fission-product release criteria.

New alpha-counting apparatus is being used. The counter consists of four detectors* each having an area of about 400 mm² and arranged in a cloverleaf design. The counting system has a resolution of about 75 kev (full width at half maximum). The new apparatus has greater sensitivity and better resolution than the apparatus used previously.

Several samples of coated particles have been investigated, and considerable variety in the type of spectra has been observed. Typical spectra are shown in Figs. V.1, V.2, and V.3.

Figure V.1 shows the spectrum for a standard containing Am²⁴¹ and U²³⁵ (U²³⁴). The spectrum consists of a peak at 5.48 Mev for Am²⁴¹ and a peak at 4.77 Mev for U²³⁴. (The alpha activity for enriched uranium comes mainly from the U²³⁴ isotope.) It can be seen in Fig. V.1 that the peaks are relatively sharp.

Figure V.2 shows the uranium peak for a sample of TRISO coated particles (Lot 4000-218E).† The peak shows considerable degradation (i.e., the peak is quite broad). The interpretation of this broad peak is that the uranium contamination is almost uniformly spread throughout the outer 10 to 20 μm of

*Partially depleted silicon surface barrier detectors.

†Particle description: TRISO coated carbide particles Lot 4000-218E; kernels were 225 μm (avg), (Th,U)C₂, Th:U = 2.8:1, coatings were TRISO II 52 μm buffer, 18 μm inner PyC, 19 μm SiC, 37 μm outer PyC; 8.29% uranium content.

Fig. V.1--Alpha spectra of standard containing Am^{241} and U^{235} (U^{234})

Fig. V.2--Alpha spectrum for bulk sample of TRISO coated particles (Lot 4000-218E)

Fig. V.3--Alpha spectrum for bulk sample of
BISO coated particles (Lot 4000-174E)

the outer pyrolytic carbon coating. (The range in pyrolytic carbon of alpha-particles having an energy of about 5 Mev is about 15 μm). The peak broadening is caused by energy loss of the alpha particles as they penetrate the outer coating. A concentration profile for uranium in the outer pyrolytic carbon layer for this batch of particles was measured as discussed in the following section, and the profile, which is shown in Fig. V.4, shows that the uranium concentration was relatively high to a depth of about 10 μm and appeared to peak at about 10 μm . This is consistent with the broad alpha peak observed for the particles.

Figure V.3 shows the uranium alpha peak for a sample of BISO coated carbide particles (Lot 4000-174E).^{*} The peak is relatively sharp, indicating very little alpha-energy degradation. The interpretation of this is that the uranium contamination is located on or very near the coating surface with a sharp dropoff in concentration inside the coating. This interpretation is consistent with the uranium concentration profile, shown in Fig. V.5 for this lot of particles.

The alpha-counting data indicate that the level of uranium contamination at or near the coating surface was a factor of approximately two higher for the TRISO coated particles (Lot 4000-218E) than for the BISO coated particles (Lot 4000-174E). This is consistent with the concentration profile data given in Figs. V.4 and V.5 in the following section.

These and other examples indicate that the alpha-counting procedure is promising for use in predicting uranium concentration in the outer 10 to 20 μm of coated fuel particles. A drawback is that counting times associated with 1 to 2 g of fuel particles in the present alpha-counting equipment may be too long for the technique to be useful for testing production particles.

The use of alpha scintillation counters is presently being investigated. This would enable the use of much larger samples of particles with correspondingly shorter counting times. However, no energy discrimination is possible with a system of this type, and therefore only a gross alpha-count of all the activity will be possible. Work is also under way to gain more information on the correlation of alpha-counting data with measured fission-gas release values.

DISTRIBUTION OF URANIUM AND THORIUM IN PARTICLE COATINGS

Concentration profiles for uranium and thorium in the outer pyrolytic carbon coatings of coated particles were measured for a sample of BISO coated carbide particles (Lot 4000-174E) and for a sample of TRISO coated carbide particles (Lot 4000-218E). The main purpose of this work was to characterize the distribution of uranium and thorium in the outer coatings of these fuel particles, which were used in the alpha-counting studies.

^{*}Particle description: BISO coated carbide particles Lot 4000-174E; kernels were 450 μm (avg), (Th,U) C_2 , Th:U = 5:1; coatings were 48 μm buffer, 84 μm isotropic PyC; 8.84% uranium content.

Fig. V.4--Concentration profiles for U and Th in outer pyrolytic carbon layer of TRISO coated carbide particles (Lot 4000-218E)

Fig. V.5--Concentration profiles for U in outer pyrolytic carbon layer of BISO coated carbide particles (Lot 4000-174E)

The coated particle samples were spun around by means of a gas jet over abrasive paper, and the abraded pyrolytic carbon dust was collected on filter paper. The dust samples were then activated in the TRIGA reactor and gamma-counted to determine the uranium and thorium concentrations. The resulting concentration profiles are given in Figs. V.4 and V.5. The outer pyrolytic carbon layer of the BISO coated particles shows an exponential decrease in concentration from the outside towards the center of the particles. The outer pyrolytic carbon layer of the TRISO particles shows a small increase in concentration in the first 10 to 15 μm with a subsequent exponential decrease.

The data show that the surface concentration for the TRISO coated particles is higher than that for the BISO coated particles by a factor of about four. This is consistent with the results of alpha-counting studies (see the previous section).

THERMAL STABILITY OF COATED PARTICLES

The purpose of this work is to determine the maximum fuel operating limits of time and temperature that are imposed by chemical interactions between the kernel and coating in coated fuel particles. Effort has been concentrated on out-of-pile heating experiments in which Th-U content of the kernel, nominal temperature, thermal gradient, and time are the primary variables. The condition of the coated particles is monitored by periodic microradiographic or metallographic analysis.

Thermal Gradient Experiments

Beds of coated particles are being heated in a thermal gradient and examined at intervals to detect kernel migration and changes in distribution of Si, C, Th, and U.

The apparatus and specimens of experiment 4283-72 were described in a previous quarterly report (GA-9261). Heating of the seven TRISO samples continued in a thermal gradient of about $0.03^\circ\text{C}/\mu\text{m}$ at a nominal temperature of 1400° to 1550°C , and the specimens were examined after 8217 hr of heating. Kernel migration toward the hot side of the particle (amoeba effect) is observed in 50% to 75% of the particles.

Determination of the chemical difference between the migrating and non-migrating kernels remains to be done. If a significant difference can be identified, some degree of control over kernel migration may be possible.

Analysis of migration rate data in this experiment is incomplete; nevertheless, results from three of the TRISO samples are summarized in Table V.1. (For Comparison some BISO samples are also shown.) Heating of particles is continuing with time at temperature now at 8500 hr.

Experiment 4464-25 is a long-term intermediate-temperature (1000° to 1300°C) test in which samples of 14 different TRISO coating batches are being heated in a thermal gradient of 0.01 to $0.06^\circ\text{C}/\mu\text{m}$. The apparatus and specimens were described in detail in a previous quarterly report (GA-9720).

Table V.1

SUMMARY OF FUEL KERNEL MIGRATION FROM HEATING TRISO AND BISO COATED
ThC₂, UC₂, AND (Th,U)C₂ PARTICLES IN A THERMAL GRADIENT

Batch	Coating Type	Kernel Th:U Ratio	Exp. No.	Part. No. ^a	Thermal Gradient (°C/μm)±30%		Nominal Temp (±50°C)	Kernel Migration Distance ^d (μm)	Time (hr)	Migration Rate (10 ⁻³ μm/hr)
					Particle ^b	Kernel ^c				
3592-35	TRISO I (LTI)	2	4464-4	1	0.060	0.011	1525	18.9	1100	17.2±1.8
				2	0.060	0.011	1520	8.1	1100	7.4±1.8
				3	0.060	0.011	1520	13.5	1100	12.3±1.8
				4	0.060	0.011	1440	8.1	1100	7.4±1.8
				5	0.060	0.011	1440	8.1	1100	7.4±1.8
			4464-35	6	0.040	0.007	1730	25	32	780±60
				7	0.030	0.005	1600	10	32	312±60
				8	0.030	0.005	1590	5	32	156±60
			4283-72	9	0.045	0.008	1750	25	6377	4.0±0.3
				10	0.045	0.008	1750	20	6377	3.1±0.3
				11	0.045	0.008	1750	31	6377	4.9±0.3
				12	0.045	0.008	1750	20	6377	3.1±0.3
				13	0.045	0.008	1525	30	6377	4.7±0.3
				14	0.045	0.008	1525	30	6377	4.7±0.3
				15	0.137	0.024	1450	10	6377	1.6±0.3
				16	0.137	0.024	1450	15	6377	2.3±0.3
3592-151	BISO (HTI)	3.5	4464-4	1	0.060	0.011	1500	10.8	1100	9.8±1.8
				2	0.060	0.011	1500	5.4	1100	4.9±1.8
				3	0.060	0.011	1420	8.1	1100	7.4±1.8
			4283-72	4	0.030	0.005	1375	2.7	2992	0.9±0.7
				5	0.030	0.005	1400	2.7	2992	0.9±0.7
4413-127	BISO (LTI)	ThC ₂	4464-35	1	0.040	0.007	1770	40	32	1250±60
				2	0.035	0.006	1650	4	32	125±60

Table V.1 (continued)

Batch	Coating Type	Kernel Th:U Ratio	Exp. No.	Part. No. ^a	Thermal Gradient (°C/μm)±30%		Nominal Temp (±50°C)	Kernel Migration Distance ^d (μm)	Time (hr)	Migration Rate (10 ⁻³ μm/hr)
					Particle ^b	Kernel ^c				
4413-127	BISO (LTI)	ThC ₂	4464-4	3	0.060	0.011	1500	60 ^e	1100	82±1.8
				4	0.060	0.011	1475	10 ^e	1100	27±1.8
				5	0.060	0.011	1450	20 ^e	1100	18±1.8
				6	0.060	0.011	1475	20 ^e	1100	27±1.8
4263-91	TRISO II (HTI)	2.2	4464-35	1	0.040	0.007	1790	50	32	1565±60
				2	0.040	0.007	1770	40	32	1250±60
				3	0.030	0.005	1615	8	32	235±60
3592-143	BISO (LTI)	3.5	4283-72	1	0.030	0.005	1400	5.5	6377	0.9±0.3
				2	0.030	0.005	1375	5.5	6377	0.9±0.3
				3	0.030	0.005	1375	2.7	6377	0.4±0.3
				4	0.030	0.005	1350	2.7	6377	0.4±0.3
				5	0.030	0.005	1350	5.5	6377	0.9±0.3
				6	0.030	0.005	1375	2.7	6377	0.4±0.3
4042-95	TRISO II (HTI) (Si doped) PyC	2.75	4283-72	1	0.045	0.008	1500	23	6377	3.7±0.3
4000-889	TRISO II (LTI)	2.75	4464-4	1	0.060	0.011	1525	8.2	1100	7.3±1.8
				2	0.060	0.011	1500	13.7	1100	12.5±1.8
				3	0.060	0.011	1500	2.7	1100	2.5±1.8
				4	0.060	0.011	1475	5.5	1100	5.1±1.8
				5	0.060	0.011	1450	2.7	1100	2.5±1.8
				6	0.060	0.011	1450	5.5	1100	5.0±1.8
				7	0.060	0.011	1450	2.7	1100	2.5±1.8
				8	0.060	0.011	1475	2.7	1100	2.5±1.8
				9	0.060	0.011	1525	11.0	1100	10.0±1.8

Table V.1 (continued)

Batch	Coating Type	Kernel Th:U Ratio	Exp. No.	Part. No. ^a	Thermal Gradient (°C/μm) ±30%		Nominal Temp (±50°C)	Kernel Migration Distance ^d (μm)	Time (hr)	Migration Rate (10 ⁻³ μm/hr)
					Particle ^b	Kernel ^c				
4000-889	TRISO II (LTI)	2.75	4283-72	10	0.030	0.005	1400	2.7	8217	0.33±0.24
				11	0.030	0.005	1375	5.5	8217	0.67±0.24
				12	0.030	0.005	1375	2.7	8217	0.33±0.24
				13	0.030	0.005	1350	5.5	8217	0.67±0.24
				14	0.030	0.005	1350	5.5	8217	0.67±0.24
				15	0.030	0.005	1375	8.2	8217	0.99±0.24
				16	0.030	0.005	1400	11.0	8217	1.33±0.24
				17	0.030	0.005	1375	2.7	8217	0.33±0.24
				18	0.030	0.005	1375	2.7	8217	0.33±0.24
				19	0.030	0.005	1350	11.0	8217	1.33±0.24
4413-132	TRISO II (LTI) (N ₂)	UC ₂	4464-4	20	0.030	0.005	1375	11.0	8217	1.33±0.24
				1	0.060	0.011	1525	5.5	1100	5.0±1.8
				2	0.060	0.011	1500	5.5	1100	5.0±1.8
				3	0.060	0.011	1475	2.7	1100	2.5±1.8
				4	0.060	0.011	1450	2.7	1100	2.5±1.8
				5	0.060	0.011	1500	2.7	1100	2.5±1.8
				6	0.060	0.011	1525	2.7	1100	2.5±1.8
				7	0.060	0.011	1500	2.7	1100	2.5±1.8
4423-3	TRISO II	UC ₂	4464-4	8	0.060	0.011	1475	2.7	1100	2.5±1.8
				1	0.060	0.011	1525	5.5	1100	5.0±1.8
				2	0.060	0.011	1475	2.7	1100	2.5±1.8
				3	0.060	0.011	1475	2.7	1100	2.5±1.8
				4	0.060	0.011	1500	2.7	1100	2.5±1.8
				5	0.060	0.011	1525	5.5	1100	5.0±1.8
				6	0.060	0.011	1500	2.7	1100	2.5±1.8
				7	0.060	0.011	1500	2.7	1100	2.5±1.8
				8	0.060	0.011	1500	2.7	1100	2.5±1.8

^aParticles showing no detectable migration (25% to 50% of total) were not included in this summary.

^bDetermined from measurement of macro thermal gradients in crucibles holding particles.

^cKernel thermal gradient assumed to be about 18% of particle thermal gradient.

^dMigration toward the hot side determined by measuring thickness of graphite precipitate on the kernel cool side.

^eMigration toward hot side determined by measuring the advance of the hot kernel surface, the cool surface remaining stationary.

The specimens were examined at about 2000 hr of heating, and no significant change from the as-coated condition was observed. Heating is being continued, and the time at temperature has now reached 2700 hr.

Temperature and Thermal Gradient Dependence of Kernel Migration

From consideration of irreversible thermodynamics and the assumption that thermal diffusion controls the rate of migration, the theoretical relationship between kernel migration rate, temperature, and thermal gradient has been shown to be

$$\frac{dx}{dt} = K D_c \frac{dT}{dx} \frac{1}{T^2} \quad , \quad (1)$$

where $\frac{dx}{dt}$ = migration rate of kernel,

K = constant containing fundamental material property constants,

D_c = carbon diffusion coefficient,

$\frac{dT}{dx}$ = thermal gradient in the kernel, and

T = temperature, °K.

In this expression the driving force due to the free energy difference between PyC buffer and rejected graphite is considered to be negligible.

Considering carbon diffusion as a thermally activated process, the carbon diffusion coefficient can be expressed as:

$$D_c = D_o e^{-\frac{\Delta H}{RT}} \quad , \quad (2)$$

where D_o = pre-exponential constant,

ΔH = activation energy for carbon diffusion in the kernel,

R = gas constant, and

T = temperature, °K.

By substituting Eq. (2) into (1), one obtains

$$\frac{1}{D_o e^{-\frac{\Delta H}{RT}}} = K \frac{dT}{dx} \frac{1}{T^2} \frac{1}{\frac{dx}{dt}} \quad . \quad (3)$$

The form of Eq. (3) suggests that a plot of reciprocal temperature $1/T$ versus the logarithm of the product

$$\frac{dT}{dx} \frac{1}{T^2} \frac{1}{\frac{dx}{dt}} \quad ,$$

should produce a straight-line relationship. The activation energy for the rate-controlling diffusion process can be obtained from the slope of the line.

The migration rate data on single particles from three thermal gradient heating experiments given in Table V.1 were analyzed and plotted as suggested by Eq. (3). The results of that work are shown in Fig. V.6.

A line representing a least-squares fit of the data was drawn through the data points. A large temperature dependence of migration rate was reflected in the activation energy of 136 kcal/mole, which was calculated from the slope of the line. This activation energy is much larger than the activation energies that have been measured for diffusion of carbon in carbides of uranium, 63.5 to 98.3 kcal/mole (Ref. 1). No explanation is offered at this time for the high measured activation energy. However, it suggests that simple carbon diffusion does not by itself control the kernel migration rate.

Although there was considerable scatter in the data, the migration rate of UC_2 kernels was generally less than the average for mixed carbides, and the migration rate of ThC_2 kernels was greater than the average for similar temperature and thermal-gradient conditions. Additional data are desirable to confirm the trend indicated here. The range of thermal gradients employed in these experiments was from 0.005 to 0.024°C/ μ m, but there was no systematic study of the effect of thermal gradient on migration rate at a single nominal particle temperature. Therefore, definite conclusions regarding the temperature gradient dependence of migration cannot be made at this time. However, it is possible to predict the migration rate under the influence of various thermal gradients based on the theoretical linear dependence of migration rate on thermal gradient. Figure V.7 shows the surface for the relationship between migration rate, thermal gradient, and temperature, based on the data in Fig. V.6, and the theoretical relationship of Eq. (1). In Figs. V.6 and V.7 are shown the strong temperature dependence of the migration rate. For example, at a constant thermal gradient the migration rate increases by a factor of about 800 when the temperature is increased from 1300° to 1500°C, and a corresponding decrease in particle coating lifetime can be expected.

ANALYTICAL STUDIES OF COATING DESIGN

Studies of TRISO coated particle design are being conducted using a stress-analysis model developed by J. L. Kaae (Refs. 2,3). The Kaae model is a mathematical analysis of coating stresses induced by the buildup of fission-gas pressure, anisotropic radiation-induced dimensional changes in the inner and outer pyrolytic carbon (PyC) layers, and differential thermal expansion of the SiC and PyC layers. The analysis also accounts for the stress-relieving influence of radiation-induced creep in the PyC layers. Irradiation-induced dimensional change and creep parameters for high-temperature isotropic (HTI) PyC are those reported by Bokros, *et al.*, in Ref. 4. The remainder of the input parameters are the same as those reported in an earlier quarterly report, GA-8879.

Fig. V.6--Thermal gradient and temperature dependence of kernel migration in ThC₂, UC₂, and (Th,U)C₂ kernels of coated particles

Fig. V.7--Temperature and thermal gradient dependence of migration rate of $(\text{Th,U})\text{C}_2$ kernels in BISO and TRISO coated particles

All of the design calculations to date have been based upon HTI pyrolytic carbon data. However, dimensional change data for low-temperature isotropic (LTI) PyC have recently become available (see Ref. 5). Data for LTI PyC densities of 1.55 to 2.0 g/cm³ at 600°C and 1.7 to 2.0 g/cm³ at 1000°C have been compiled in a form suitable for use in the Kaae-model calculations; the 1000°C data are shown in Fig. V.8. Comparisons between LTI and HTI dimensional changes at the two temperatures are shown in Figs. V.9 and V.10. Results of the first model calculations using LTI data indicate that the LTI coatings have lower stresses than the HTI, but greater dimensional change.

FISSION-PRODUCT RELEASE

Long-term (TRIGA Mark III) Fission-gas Release Studies

Apparatus is being set up for performing long-term fission-gas release measurements utilizing a King (carbon-resistance) furnace mounted in the TRIGA Mark III reactor. The purpose of this work is to determine the effect of time, temperature, and possible fuel migration on the steady-state gas release from coated fuel particles.

References

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Fig. V.8--Irradiation-induced dimensional changes of LTI pyrocarbon at 1000°C

Fig. V.9--Dimensional changes of HTI and LTI PyC

Fig. V.10--Dimensional changes of HTI and LTI PyC