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

QUARTERLY PROGRESS REPORT
FOR THE PERIOD ENDING
SEPTEMBER 30, 1969

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

October 29, 1969

P4766

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Gulf General Atomic Incorporated

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GA-8879-July, 1968, through September, 1968
GA-9130-October, 1968, through December, 1968
GA-9261-January, 1969, through March, 1969
GA-9440-April, 1969, through June, 1969

INTRODUCTION

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

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

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PART I

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

Task I
COMPONENT DEVELOPMENT

REACTOR VESSEL

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

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

PCRV Model No. 2

Computer plots of pressure versus strain were generated from data obtained during the 1200- and 1500-psig overpressure tests of PCRV Model No. 2. The data were produced by strain gages in the concrete, on the reinforcing steel, and on the liner. The plots will be incorporated in a supplemental report on the PCRV Model No. 2 tests.

Prestressing System

A report on the investigation of the corrosion-protection system discussed in the previous quarterly report has been published (Ref. 1).*

Concrete Properties

Evaluation of the data from the concrete properties tests continued during the quarter.

AUXILIARY EQUIPMENT AND SPECIAL TOOLS

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

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

Reactor Viewing Device

Functional tests of the reactor viewing device in air were completed. The purpose of this part of the test program was to confirm that all components of the reactor viewing device as installed on the fuel handling machine can perform their design mechanical and electrical functions. The actual viewing device components were used, with the exception of the viewing mirror. A functionally equivalent mirror was built as a substitute for the tungsten alloy unit, which has not yet been received.

In addition to the equipment functional tests, a series of tests was run and recorded photographically to determine optimum lighting and viewing angle conditions. A test specimen of a simulated fuel element crack was used for a portion of the test program.

The functional tests of the television camera disclosed the presence of vertical lines masking the extreme left hand side of the picture. According to the manufacturer this is caused by capacitances between adjacent conductors as a result of the very long cable between the camera and preamplifier. There is no practical way to remedy this, and since the picture area affected is quite small, it can be tolerated.

During the course of the tests, the camera temperature rose from 70° to 120°F as a result of the adjacent lights. This resulted in a small but noticeable degradation of picture quality. Figure 1.1 shows an object viewed when the camera was at 79°F.

The tests to determine the optimum lighting arrangement indicated that a single light provided the best viewing conditions at all distances. This was because of unexplained horizontal bands appearing on the television monitor when additional lights were turned on. Figure 1.2 shows this effect.

Identification of cracks became more difficult as the angle between the line of sight and the plane of the cracked graphite surface decreased, as may be seen by comparing Figs. 1.3 and 1.4.

Reserve Shutdown Vacuum Tool

The initial reserve shutdown vacuum tool development tests showed that the suction head and helium flow developed by the original three vacuum blowers operating in parallel were inadequate to pick up the reserve shutdown material. Accordingly, an exhaustive search was initiated to locate a high-performance blower unit that would fit into the available space envelope.

A relatively inexpensive positive-displacement blower (capable of providing the predicted flow required to pick up the reserve shutdown material) was used in a test to verify that this flow would actually be adequate. Accordingly, a rotary positive-displacement blower was connected to the existing test facility, as shown in Fig. 1.5. A 4-in. flexible hose connecting the vacuum tool canister to the external blower circuit permitted sufficient vertical movement of the test unit to feed the pickup probe down the simulated channel where it could pick up the graphite balls. The vacuum canister was supported by a rod that

Fig. 1.1--View of object with camera at 79°F

Fig. 1.2--View showing horizontal bands on monitor screen

Fig. 1.3--Photo taken with viewing angle of 90°

Fig. 1.4--Photo taken with viewing angle of 45°

penetrated the top of the autoclave through a sealing gland. The canister and probe assembly were raised and lowered by an air-operated hoist.

The instrumentation, as shown in Fig. 1.5, consisted of a pitot tube mounted in the inlet line to the blower and a pressure tap to the autoclave interior. Valves in the instrumentation lines permitted measuring the velocity head by means of an inclined manometer, and the absolute pressure at the pitot tube and in the autoclave by means of a mercury manometer.

Two test runs were made, each at a different blower speed. Both runs were made with the helium temperature at 67°F and with a pressure at 8.9 psia to simulate the helium density at 250°F and 14 psia. The results of the test (shown in Table 1.1 and in Fig. 1.6) include the total pressure drop of the test rig flow circuit.

Table 1.1
RESULTS OF RESERVE SHUTDOWN VACUUM TOOL TEST

Run No.	Blower Speed (rpm)	Velocity Head (In. H ₂ O)	Press. at Pitot Tube (In. Hg)	Press. at Autoclave (In. Hg)	Blower ΔP (In. Hg)	Flow (CFM)
1	2030	0.28	15.6	18.5	2.9	658
2	1180	0.14	16.8	18.1	1.3	465

With the flow produced in run No. 1, the 1/2-in. graphite balls were readily picked up when the probe was lowered. With the probe stationary, the balls were picked up at a distance of 1 in. from the tip of the probe. With the lower flow conditions in run No. 2, the balls were picked up, but only marginally; several balls, which apparently were in a state of balanced suspension within the pickup probe, dropped out when the blower was shut off. This is probably due to the fact that this blower, being a constant volume machine, would create sufficient pressure drop across a large number of balls to accelerate them up the probe, but if the probe lowering is stopped, the last few balls pickup up would create only enough pressure drop to keep them suspended in the probe.

It can be concluded from the results of the test that a blower capable of creating a suction head of at least 40 in. of water with a helium flow of 650 ft³/min at helium conditions of 14 psia and 250°F would be adequate.

Manipulator

During a brief interruption in the fuel handling machine test program, the manipulator was installed on the fuel handling machine and an operational check was made to check out the control circuitry. Some minor circuitry changes were required to make the manipulator completely operational. After a brief period of operator practice with the manipulator, it was removed and stored.

Fig. 1.5--Reserve shutdown arrangement for the reserve shutdown vacuum tool test

Fig. 1.6--Reserve shutdown vacuum tool helium flow characteristics, flow versus blower head

ORIFICING MECHANISM

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

The orificing mechanism was thoroughly inspected, and no undue increase in friction could be found to account for the intermittent stall condition discussed in the previous quarterly report (GA-9440). It was concluded, therefore, that the motor provided insufficient power to handle the inherent stiffness in the system.

Installation of a motor with a higher power sufficiently improved operation that the test target of 222 valve cycles was satisfactorily achieved. There was, however, some evidence of stalling on subsequent cycles. A stall test on the motor showed the output torque to be only about 70% of the rated value.

It was concluded that a deterioration of the magnetic field had occurred when the rotor was removed to install the solid-film lubricated bearings. The motor was remagnetized by the manufacturer, and performance was found to be satisfactory.

The test program was concluded with the performance of an additional 25 valve cycles under environmental conditions during which performance was satisfactory.

The full-scale orifice flow and vibration test has been completed for the revised orifice design, and the data have been analyzed. Data were taken to relate orifice setting, Z , to flow rate, m , and the orifice pressure loss coefficient, K_{orif} , for a constant pressure drop through the test structure. Flow and orifice closure were normalized with respect to their maximum values, and m/m_{max} was plotted against Z/Z_{max} , as shown in Fig. 1.7. The quantity Z/Z_{max} measures the closure of the orifice valve ($Z/Z_{\text{max}} = 1.0$ in the full-closed position), while m/m_{max} represents the mass flow rate normalized with respect to flow through the full-open orifice. This graph indicates a 10% change in mass flow rate for the first one-half of the orifice valve travel and a 25% change in flow for the last 10% of travel. This information is related to the change in outlet temperature (the dependent variable of concern here) by the heat balance equation $q = mC_p\Delta T$, and the flow rate is found to be inversely proportional to $\Delta T = (T_{\text{out}} - T_{\text{in}})$. With all other variables held constant, 75% of the outlet gas temperature control is obtained during the last 50% of orifice travel distance. However, for this design the pressure loss through the fully open orifice is minimized to allow maximum flow into maximum power regions.

Figure 1.8 shows how well region coolant outlet temperatures are controlled by the orifice valve when 83 adjustment steps per inch of axial motion are employed; curves where expected crossflow occurs and no crossflow occurs are shown. The change in outlet coolant temperature per orifice adjustment step, ΔT_{out} , divided by the radial peaking factor, P_r , is plotted against orifice pressure loss coefficient.

Figure 1.8 reveals that the maximum sensitivity will occur in regions with nearly closed orifices ($K_{\text{orif}} > 300$), which are also the low-powered regions,

Fig. 1.7--Mass flow rate versus orifice setting

Fig. 1.8--Change in outlet coolant temperature per orifice adjustment step versus orifice pressure loss coefficient

if there is no crossflow. Crossflow is expected to be at a minimum in the initial core.

In interpreting this figure, it should be remembered that the orifices on high-powered regions, $P_r > 1$, are always nearly open ($K_{orif} < 50$) and the orifices for low-powered regions, $P_r < 0.8$, are nearly closed ($K_{orif} > 200$). For any realistic combination of orifice setting and radial peaking factor, the maximum change in outlet gas temperature for a one-step change in orifice position is expected to be 20°F where there is no crossflow and less than 10°F where crossflow occurs. These values are well within the design objective for the orifice.

References

1. Hildebrand, J. F. Tests Relating to Corrosion Protection for Tendons of the Public Service Company of Colorado Prestressed Concrete Reactor Vessel, USAEC report GA-9356, Gulf General Atomic Incorporated, August 6, 1969.

Task II

NUCLEAR ANALYSIS

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.

The single-channel synthesis-depletion analysis of Reference Design 7 has been described in some detail in the two previous quarterly reports, GA-9261 and GA-9440. Reference Design 7 was characterized by a two-year initial cycle and a buffer zone one fuel element thick at the core-reflector interface. This design iteration appeared to be well within design limits considering refueling region peaking factors, stability of the axial power distribution, and core reactivity requirements and shutdown capability.

Although the over-all nuclear characteristics of Reference Design 7 appeared to be acceptable, further analysis has shown problems of flux tilting across refueling regions and high fast fluence in the central portion of the core, resulting from the use of the thick buffer zone at the core-reflector interface.

Power Tilting Across a Refueling Region

In the Fort St. Vrain reactor, the coolant flow through each refueling region is controlled by orifices to reduce maximum fuel temperatures and to give, approximately, a constant gas outlet temperature from all regions in the core. The average gas outlet temperature from a refueling region thus depends mainly on its total power output and the average coolant flow. If all seven columns in a region show the same relative peaking factor as the region average, uniform fuel temperatures across the region will result. However, if the peaking factors of single columns are considerably larger than the region average, then those columns will also show higher-than-region-average fuel temperatures.

When the thick buffer zone was adopted for Reference Design 7, the main attention was given to the local peak at the core-reflector interface (see earlier quarterly report GA-8879). In order to limit that local peak, the average power density in the outer ring of fuel elements had to be reduced. At the same time, however, the average refueling region peaking factors were kept essentially unchanged. This is illustrated in Fig. 2.1, which shows the average power density of Region 21 during a six-year cycle from Reference Design 6, using a thin four-row-thick buffer zone, and from Reference Design 7, using a thick buffer zone. The power history for the average of the region

Fig. 2.1--Power history of region 21

is quite similar for the two cases. In Fig. 2.2 the ratio of the peaking factor in the hottest column in that region to the region-average peaking is plotted. Here it can be seen that the thick buffer zone design exhibits a larger power tilt at any time in life than is obtained from the design with the thin buffer zone indicating that a limit on the refueling region peaking alone does not guarantee that maximum fuel temperatures are not exceeded.

A compilation of tilting factors in a region versus the region-average factor is shown in Fig. 2.3. These data have been compiled from the two-dimensional depletion studies of Reference Designs 6 and 7. This graph indicates, in general, that tilting across a refueling region increases as regions become older and the region peaking factor decreases. It can also be seen that this effect is considerably more pronounced in the design with the thick buffer zone. From these results it appears to be obvious that the advantages of a thick buffer zone can only be retained if the design can be altered somehow so that flux tilting can be minimized. Flux tilting factors across a region appear acceptable if their magnitude is about 1.3 or less for short periods of time (less than 100 days) and if the average tilting over the entire life is about 1.1 or less.

Fast Flux Doses

As a result of the low average power density in the outer ring of fuel elements, which comprise approximately 20% of the total core volume, a noticeable increase in power density in the central core regions was observed for Reference Design 7. Since, at a constant graphite density, the fast flux level is directly proportional to the core power density, the total integrated fast-flux dose increased correspondingly in the central core regions. Maximum fast fluences of 8.2×10^{21} nvt were calculated for the Reference Design 6, with less than 1.5% of the total core fuel obtaining doses above 8×10^{21} nvt. For the Reference Design 7, maximum fast flux doses above 8.5×10^{21} nvt were calculated, with more than 5% of the fuel exceeding 8×10^{21} nvt. Since the fuel irradiation program was designed to cover maximum fast fluences of about 8×10^{21} nvt, the more than 5% increase shown in the Reference Design 7 appears to be undesirable. However, it should also be pointed out that the heavy metal burnup limits of 20% FIMA for fissile and 7% FIMA for fertile particles would not be exceeded.

Comparison of Thin and Thick Buffer Zone Design

The main incentive for increasing the buffer zone of the core-reflector interface to encompass a complete fuel element was to simplify fuel element fabrication and fuel element loading into the reactor. With the thin buffer zone, a variety of fuel element types result, mainly because about 20% of the fuel elements contain more than one fuel type and their proper orientation in the reactor becomes important. In addition, the thick buffer zone offers advantages in the nuclear design. It reduces calculational uncertainties and, above all, shows considerably lower worths for single stuck control rod pairs. The maximum-worth rod pair with the thick buffer zone was calculated to be $0.043 \Delta k$, as compared to 0.074 for the thin buffer zone. Comparable values

Fig. 2.2--Maximum tilting in region 21

Fig. 2.3--Maximum observed tilting in any refueling region

for the maximum worth of two stuck rod pairs are 0.082 Δk and 0.122 Δk , respectively. This difference in single rod-pair worth stems from the fact that the highest rod worths are observed in the region adjacent to the core-reflector interface. The effect of the reflector on control rod worth is significantly reduced by the large buffer zone.

Major problems for the thick buffer zone, as pointed out above, are fast-flux exposure and power tilting across refueling regions. Although the gross core power distribution, as judged by refueling region peaking factors, can be kept well within design limits, the tilting problem makes a thick buffer zone unacceptable at the present time. It should also be pointed out that this conclusion is reached on the basis of using peaking factors averaged over a fuel element column. The importance of tilting across a fuel element column must still be resolved. A first attempt to obtain an answer to this question was made by the use of fine-mesh depletion calculations, which compute power histories for regions as small as 1/24 of a fuel element. The physics analysis has been completed, and a thermal analysis using these fine-mesh power histories is beginning shortly. It is only after this analysis that the final judgement on whether or not to retain a thick buffer zone can really be made. It may well be possible that local power peaks at the core-reflector interface deplete rapidly enough that a thick buffer zone could be used by raising its power density and obtaining only a modest tilt across the outer refueling regions.

Reference Design 9*

When the problems associated with the thick buffer zone became apparent, two possible alternatives were considered: (1) readoption of the thin buffer zone and (2) raising the power density in the thick buffer zone and purposely accepting an increase in power peaking at the core-reflector interface. The former approach appears most desirable at this time, although further work may show a thick buffer design to also be acceptable.

To eliminate seven-year-old fuel from the initial cycle, the initial core lifetime was reduced from two years to one for Ref. Design 9. As a result, a fuel loading reduction was required.

The characteristics of Reference Design 9 are thus:

Initial cycle	1 year
Maximum fuel lifetime	6 years
Buffer zone at core reflector interface	5 rows of fuel holes
Initial core loading:	
Thorium	19458 kg
Uranium	882 kg

*Reference Design 8 is the designation applied to the study of the four-year fuel cycle discussed in the previous quarterly report (GA-9440).

The total loading reduction actually consists of two components. First, there is the reduction in cycle time. Secondly, recent measurements on production runs of graphite logs indicate that the average fuel element carbon density is more like 1.78 g/cm^3 rather than 1.72 g/cm^3 , as assumed in previous design iterations. Since the core is slightly undermoderated, the addition of carbon to the core raises the core reactivity.

The fissile and fertile material distribution for the Reference Design 9 initial core is quite similar to that employed in Reference Design 7. A total of 13 fuel compositions are used, as shown in Table 2.1; all core regions are axially zoned with the exception of the thin buffer zone at the core reflector interface, which is axially and azimuthally uniform.

Table 2.1
FUEL LOADING FOR INITIAL CORE
(Reference Design 9)

Composition	Location, Refueling Region	Uranium (kg)	Thorium (kg)
1	1,4,5,8,10,15,17,21,25,28,32,35,36 (upper half)	112.5	3570
2	1,4,5,8,10,15,17,21,25,28,32,35,36 (lower half)	86.0	3191
3	2,6,7 (upper half)	44.3	773
4	2,6,7 (lower half)	32.7	661
5	9,11,12,14,16,19 (upper half)	100.5	1605
6	9,11,12,14,16,19 (lower half)	74.6	1406
7	20,23,24,26,27,30,31,34,37 (upper half)	136.3	1817
8	20,23,24,26,27,30,31,34,37 (lower half)	103.2	1617
9	3,13,18 (upper half)	40.9	874
10	3,13,18 (lower half)	29.2	727
11	22,29,33 (upper half)	38.9	725
12	22,29,33 (lower half)	32.3	626
13	Outer ring of 5 rows of fuel holes at core-reflector interface	50.7	1866
	TOTAL	882.0	19458

Feed requirements for reload segments are very similar to previous design iterations, since the C/Th = 225 equilibrium fuel cycle has been retained. A summary of the loadings for the first six reload segments is given in Table 2.2.

Table 2.2
FEED REQUIREMENTS FOR RELOAD SEGMENTS
(Reference Design 9)

Reload	Thorium (kg)	Uranium (kg)
1	2358	182.7
2	2358	218.9
3	2358	236.2
4	2358	238.8
5	2968	245.8
6	2358	214.5

With the reduction of the uranium loading, more volume becomes available for fuel particles. Current estimates indicate that a particle packing fraction of 60% is not exceeded for all fuel compositions.

The power distributions calculated for Reference Design 9 are well within design limits and comparable to those published for Reference Design 7. Maximum power tilting factors are shown in Fig. 2.4 and are just slightly higher than those for the Reference Design 6.

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.

Analysis of Rod Withdrawal Accidents

The largest reactivity insertion rates for the Fort St. Vrain reactor result from postulated withdrawal of control poison. All other possible reactivity sources are smaller in magnitude and of less significance.

The maximum worth of a control rod pair for the operating core is calculated to be less than $0.016 \Delta k$ for normal control rod configurations in the initial core. The maximum rod-pair worth during the equilibrium cycle is calculated to be $0.016 \Delta k$ at the beginning of the cycle and $0.012 \Delta k$ at the end of the cycle. Although the larger rod worth is calculated for the beginning-of-cycle condition, the more negative temperature coefficient at this time in life make this accident condition less severe than a $0.012 \Delta k$ rod withdrawal accident at the end of the cycle, when temperature coefficients are least negative.

A series of calculations was performed to evaluate the sensitivity of rod withdrawal accident consequences to control rod worths, and to obtain information about how uncertainties in rod worth calculations can affect the

Fig. 2.4--Reference Design 9 fuel element column tilting

calculated fuel and gas outlet temperatures. To cover the range of interest, rod worths of 0.01, 0.016, and 0.025 Δk were chosen together with the least negative temperature coefficient occurring at the end of the cycle. To emphasize the conservatism of this analysis, the temperature coefficients were taken from the four-year C/Th = 275 fuel cycle, which is more lightly loaded and shows less negative temperature coefficients than the reference C/Th = 225 fuel cycle. The above-mentioned rod worths were taken from typical control rod patterns. In order to create a 0.025 Δk worth, an out-of-sequence artificial pattern was created to obtain appropriate hot-channel peaking factors.

The analysis of the average core behavior was carried out with the point kinetics BLOOST code (Ref. 1), which gives the power history of the average core during the accident. For the hottest channel, which would give the highest fuel temperature and gas outlet temperature, the effect of gross power tilting was determined as described below.

During normal full-power operation, the coolant flow through each refueling region is adjusted by orifices to obtain approximately equal gas outlet temperatures from all refueling regions. During a rod withdrawal accident, flux tilting causes the power density in the region from which the control rod pair is withdrawn (and in adjacent regions) to increase more rapidly than the average core power density. While this tilting occurs, the orifice setting is assumed to be unchanged and the power-to-flow ratio in the affected refueling regions is strongly disturbed.

The relative change of the power density in any refueling region due to flux tilting is also a function of the inserted reactivity. Typical data are shown in Fig. 2.5 for the different control rod worths considered in this study. The peaking factors shown are believed to be an upper limit of the relative power change due to flux tilting in any region of the core. Although the maximum worth rod pair in any operating configuration is only 0.016 Δk , some out-of-sequence patterns have been used to obtain the data for higher control rod worths.

The data in Fig. 2.5 were calculated with two-dimensional static diffusion calculations neglecting any negative temperature feedback, which typically would reduce the maximum peaking factor by 10% or more. Since the rod withdrawal time is very long compared to the neutron lifetime, the core is always in a quasi-asymptotic state, and the peaking factor steadily increases to its maximum, without any overshoot.

The peaking factors shown in Fig. 2.5 were used in the temperature analysis for the hottest channel and superimposed on the rise of the average core power density; as such, they are believed to give a conservative upper limit of temperature increases to be expected during a rod withdrawal accident.

The following scram actions were assumed to occur:

1. At 140% power due to an automatic scram signaled by the flux controller.
2. 60 sec after accident initiation, due to manual action by the operator.

Fig. 2.5--Peaking factors during rod withdrawal accident for hottest channel

3. 105 sec after accident initiation. For the maximum worth rod at the end of the cycle, 0.012 Δk , the scram signal on reheat steam temperature of 1075°F would be initiated at this time point. For the higher rod worths analyzed, the scram on reheat steam temperature would occur earlier. However, a scram 105 sec after the accident commences has been assumed to facilitate the analysis.

Figure 2.6 shows the maximum rise in average fuel and average gas outlet temperature. If the scram on 140% power terminates the transient, average fuel and average gas outlet temperature rise by less than 100°C and 100°F, respectively, and the effect of rod worth is almost negligible. If the scram is determined by the time after accident initiation at which it occurs, results become fairly sensitive to the reactivity insertion, especially if the scram is delayed until 105 sec after accident initiation. Again it should be emphasized that this latter situation has been created quite artificially and does not reflect actual plant performance. Figure 2.7 shows similar results for the hottest fuel channel. The maximum fuel temperature refers to the centerline fuel temperature in the hottest seven fuel elements in the affected refueling region and, as a total, constitutes less than 5% of the over-all core volume. Peak temperatures are very insensitive to rod worths if the scram occurs at 140% power. However, both fuel and gas temperatures are strongly affected by a delay of the scram.

For the reference accident, 0.012 Δk , the maximum fuel temperature would rise by about 1800°C. The average centerline fuel temperature in the seven hottest elements would exceed 2500°C, and the particle coatings in these elements would be expected to fail. However, 98% of the core fuel would stay below 1600°C during this transient and show no damage.

The average gas outlet temperature would rise to about 2100°F, with a maximum temperature from the hottest region exceeding 3000°F shortly. This accident would lead to some system damage but would not prevent safe shutdown of the plant.

Boron Loading in Reserve Shutdown System

The reserve shutdown system is designed to be capable of shutting the reactor down to refueling temperatures in the event of a situation that prevents the insertion of normal control poison. Its reactivity worth has to be sufficient to cover the temperature defect to refueling temperature, complete xenon decay, and the decay of Pa^{233} to U^{233} over an extended period. The minimum reactivity requirement is 0.11 Δk .

The current reference design uses 0.16 g of boron per cubic centimeter of channel in the central seven regions and 0.32 g boron/cm³ of channel in the outer 30 regions, providing a total of 0.13 Δk . In order to enhance the poison stability during a hypothetical loss-of-coolant accident it is desirable to lower the boron content in the second ring of regions, channels 8 through 19, from 0.32 g/cm³ to 0.15 g/cm³. The calculated loss in reactivity worth is about 0.005 Δk . This reduction still leaves sufficient reactivity in the reserve shutdown system to cover all requirements.

Fig. 2.6--Rod withdrawal accident from full power for average channel

Fig. 2.7--Rod withdrawal accident from full power for hottest channel

ANALYTICAL STUDIES

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

Criticality Analysis of Fresh Fuel Shipping Container

A schematic of the proposed fresh fuel shipping container is shown in Fig. 2.8. The fuel element is contained in an inner steel barrel. All void spaces between fuel element and barrel are filled with vermiculite. The inner barrel is inserted into a second steel barrel, and the void space between the two barrels is also filled with vermiculite. Each container unit contains one fuel element, and the containers are to be shipped by truck from San Diego to the reactor site.

A criticality analysis was performed to determine the maximum number of containers that can be shipped together at any time without criticality hazard under normal or accident conditions. The following conservative assumptions were used:

1. All fissile material present in an element is U^{233} .
2. No element contains burnable poison.
3. All barrels contain the most reactive fuel elements expected for Fort St. Vrain, i.e., 11.8 kg of thorium and 1.5 kg of 93.5% enriched uranium per fuel element.
4. The density of the packing material, vermiculite, is 4 lb/ft^3 , which is the lightest material offered and thus will give the smallest neutron absorption.

All calculations were carried out with one-dimensional diffusion theory or transport codes, using nine neutron energy groups, five fast and four thermal.

Assuming that the containers are infinitely long and packed in an infinite array, the multiplication constant is calculated to be about 1.4 for an unflooded condition. Since the radial flux profile calculated in unit-cell calculations was fairly flat for all neutron energy groups, disadvantage factors were neglected in calculating the multiplication constant for an infinite layer as a function of layer thickness. The results are shown in Fig. 2.9. An infinite layer two barrels high gives a calculated multiplication constant of about 0.72. Addition of a third layer of barrels raises the k_{eff} to almost 1.0. These results are not strictly conservative because of the disregard of disadvantage factors, but the neutron balances indicate that less than 10% of all absorptions occur in vermiculite or steel.

An upper limit for the multiplication constant of an infinite layer two barrels high is about 0.80, and it is concluded that two layers can be shipped on a truck without criticality hazard.

*NOTE: RADIUS OF THE CIRCLE PRESERVES AREA OF HEXAGON.

Fig. 2.8--Vertical cross section of shipping container (not to scale)

Fig. 2.9--Multiplication constant versus number of infinite layers of barrels

All criticality calculations for flooded containers were carried out assuming an infinite array of infinitely long cylinders. Figure 2.10 shows that only the addition of small amounts of water to the inner fuel-containing barrel would raise the multiplication constant slightly compared to the dry case. Any addition of water to the space between the containers increases the neutron absorption in the steel and makes the system less critical.

Figure 2.11 shows the multiplication constant for an infinite array of fully immersed cylinders at various stages of flooding of the inner and outer barrels. At no time does a larger multiplication constant result than for the dry case.

It is concluded that two layers of fuel containers can be shipped safely on a truck, and they will not become a critical system under any flooded condition.

Neutron Sources

External neutron sources are required for initial reactor startup, after refueling, and after any shutdown. The external sources are located in the upper layer of fuel elements. The present design calls for Po^{210} -Be sources for startup of the initial core and for Sb-Be sources to be relied on for later startups.

According to the current estimate of startup source requirements, a minimum effective source strength of 5×10^8 n/sec is needed. Applying a safety factor of two for calculational uncertainties and an additional safety factor of four to allow for source decay over two half lives, the total required source strength in the initial core for Po-Be sources is about 4×10^9 n/sec or about 2000 C.

Alternative neutron sources have been investigated to evaluate their feasibility and their attractiveness from a cost standpoint (see Table 2.3).

Table 2.3
POSSIBLE NEUTRON SOURCES FOR FORT ST. VRAIN REACTOR

Isotope	Po^{210}	Pu^{238}	$\text{Am}^{241}\text{-Cm}^{242}$
Source type	α, n	α, n	α, n
Half life	138d	90y	458y/163d
Neutron yield, n/sec-C	2.3×10^6	2×10^6	3.1×10^6
Max. operating temp, °F	1300	---	1500

Pu^{238} -Be sources appear attractive because of the long half life of Pu^{238} , which is about 90 years. The long half life of Pu^{238} would eliminate the safety factor of four for source decay, which is mainly used to allow for startup delay or other unforeseen shutdowns early in reactor life. The main problem with Pu^{238} sources is associated with the large thermal capture cross

Fig. 2.10--Multiplication constant versus flooding for shipping container

Fig. 2.11--Shipping containers immersed in water

section (about 500 b) of Pu^{238} . About 40% of the Pu^{238} present would burn out within the first six months.

Other possible alternatives are Am^{241} -Be- Cm^{242} sources. This type of source, which has recently become available, is particularly attractive because of its high neutron yield and high-temperature stability. An additional advantage lies in the fact that the source itself is regenerative, and it may well be possible that the reactor can be monitored for two or three cycles on the primary sources before the secondary Sb-Be sources are required.

References

1. Merrill, M., BLOOST-V, A Combined Reactor Kinetics-Heat Transfer Code for the IBM-7044, Preliminary Description, USAEC report GAMD-6644, General Dynamics, General Atomic Division, August 17, 1965.

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.

Thermal and Coolant Flow Design

Neutron Sources. Six primary locations and 24 alternative locations have been established for the startup neutron sources. The six optimum locations will be determined before startup. The initial startup sources will be located in the top layer of fuel elements in the six selected optimum fuel column locations. A special extended handling hole for placement of a source will be provided in the fuel elements. For the initial sources, a conservative maximum column radial peaking factor of about 1.75 was selected from the first year of the initial loading of the Reference Design 6 core to determine the operating temperatures for the initial sources. Similarly, for the secondary sources, a conservative maximum column radial peaking factor of about 2.10 was selected from the first seven years of the Reference Design 6 core. This peaking factor will be used for a similar thermal analysis of the secondary sources which, at present, have essentially the same configuration and expected location as the initial startup sources.

Hot Channel Factors. The effect of individually applied hot-spot factors has been determined. Engineering tolerances and physical uncertainties that were considered for this study included dimensional variations on coolant hole size and position, thermal conductivities of fuel and graphite, uncertainties in the heat-transfer correlation, fuel loading tolerance, and the uncertainties in the radial and axial power peaking factors. The fuel hot-spot factor calculated by the root-sum-squares technique increases the maximum fuel temperature from 2300° to 2536°F as a possible local peak value.

Effect of Control Rod Holes on Fuel Temperature. The effect of the control rod holes on fuel temperature has been determined. For region 24 with a radial peaking factor of 1.833, the maximum fuel temperature was found to increase from 1965° to 2138°F due to the proximity of the control rod hole. Crossflow tends to bleed coolant flow from coolant holes into the control rod holes at peak power points causing the observed peak temperature increase.

Crossflow Correlation for COPE. A systematic determination of the crossflow factors as a function of region characteristics has been completed. The present definition of crossflow factor is flow in a given fuel element divided by exit flow of the column in which the element resides. This correlation includes the effect of orifice setting, region age, and radial peaking factor and has the form

$$F_{cf} = CF_o F_a F_p ,$$

where C is a constant and F_o , F_a , and F_p are functions of orifice setting, age, and radial peaking factor, respectively. The functions have been determined for the center-core seven-column regions and for the five-column regions. The correlations have been fitted mathematically and incorporated into COPE, the core performance code, to account for crossflow at a great savings in computer time. The new version, redesignated as POKE, is presently being debugged.

Fission-product Transport

Primary Coolant Plateout Monitor System. The plateout monitor system has been reexamined with a view toward making a more quantitative determination of the amount of cleanup in each pass around the main coolant loop. It would appear possible to sample the gas on the outside of the steam generator module. This return coolant gas could be made to flow through an additional diffusion tube within the probe and discharge into the steam generator outlet plenum in the same manner as the other diffusion tubes. Thus, it would be possible to obtain from each probe a good diffusion tube analysis of both the core outlet and the circulator outlet coolant streams.

The preliminary room-temperature tests to determine the torque required to seat the metal O-ring seals have been completed. The tests indicated that the unseating torques were about 2/3 of the seating torques, and that lower leak rates could be obtained by having close fitting retaining rings. A plot of a leak rate constant versus seating torque indicated that satisfactory seals were then obtained at relatively low torque values. It has been tentatively decided that the installation torque will be specified at 200 in.-lb, which is half the rated capability of a proposed handling tool utilizing a flexible drive shaft. It has also been decided to use a ratchet type of probe latch, with shear pins for emergency removal capability. In this design all of the parts are located within the probe head to ensure retention of any broken members.

The plateout probe activity per unit area will be approximately five times higher than for the Peach Bottom probes. This will still allow direct disassembly operations but some shielding may be necessary if higher levels are encountered.

The geometry selected has a removable external plateout sleeve and five internal parallel flow paths. Four of these will be diffusion tubes sampling the core outlet coolant gas, with the inlet ports arranged to provide some information about the efficiency of dust collection. The fifth will be a diffusion tube sampling the return coolant gas. All of these flows will discharge through a coiled sorbent bed approximately 18 in. long.

Although it is intended that the probe be analyzed for Sr^{90} only, much additional insight and information could be obtained by analyzing for additional gamma-emitting species. Some candidates are 21 h I^{133} , 33 h Ce^{143} , 78 h Te^{132} , 3.9 d Sb^{127} , 8.05 d I^{131} , 12.5 d Sb^{126} , 12.8 d Ba^{140} , 32 d Ce^{141} , 34 d Te^{129m} , 285 d Ce^{144} , 2.1 y Cs^{134} , 2.7 y Sb^{125} , and 30 y Cs^{137} . The number of species that could be detected would, of course, depend on the elapsed time before counting.

Drawings of the steam generator plateout probe penetration well and the plateout probe interface criteria were completed and released. Construction drawings for the plateout probe environmental test components were also completed, and construction is proceeding. Efforts are being made to prepare an autoclave facility for these probe environmental tests.

Steam Exposure of Failed Fuel Particles. A series of simple experiments is under way to evaluate the effect of steam exposure on the mechanical integrity of fuel blocks. A 10% failure fraction of fuel particles will be simulated by having 10% of thinly coated particles in the fuel region. Heating to temperatures greater than 2000°C will cause fuel to migrate through these thin coatings more rapidly than through the regular TRISO coatings. The specimens will then be exposed to steam and the amount of unrestrained dimensional changes observed. Further engineering tests will then follow, depending on the amount of change observed.

Metallic-fission-product Release. A series of computer runs is being made with the FIPER code to calculate the theoretical concentration profiles for the GAIL IV element, the Peach Bottom D06-01 element, and other laboratory diffusion experiments. Concentration profiles are being calculated for Cs¹³⁷ and Sr⁹⁰ for the Peach Bottom element, and for Cs¹³⁷, Sr⁹⁰, and Eu¹⁵⁵ for the GAIL IV element. The laboratory diffusion experiments and calculations are being made using Sr⁸⁴.

The cesium concentration profiles obtained from the GAIL IV and D06-01 postirradiation examinations are the more abundant and apparently more reliable data. Less strontium concentration data are available because of the more complex radiochemical analysis involved. However, it is believed that europium data may be helpful, since this species shows similarities to strontium in chemical properties.

Time-dependent computer calculations were made for Cs¹³⁷ and the diffusion coefficient was adjusted to obtain the best match of the theoretical and measured concentration profiles. A present, a close match of the cesium profiles in the D06-01 sleeve graphite, the unimpregnated spine graphite, and the GAIL IV body graphite has been obtained using a single temperature-dependent diffusion coefficient. The data and calculations cover four axial locations on the D06-01 element and six axial locations on the GAIL IV element, and cover a temperature range from 500° to 1000°C. Over this temperature range, the activation energy for diffusion was found to be approximately one-tenth that previously used for cesium diffusion in graphite and may actually indicate a gas-phase transport. In order to verify these results, a reexamination of past experiments, such as the GAIL III-B fuel element, may be attempted.

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

The reference fuel element for the Fort St. Vrain reactor consists of an unpurged hexagonal graphite block containing fuel beds (columns of coated fuel particles packed to a high density). Fuel development efforts are directed towards establishing a practical and effective process for preparing beds of particles meeting the reactor design and performance requirements. In addition to process development, test and evaluation efforts are being conducted to establish various physical, mechanical, and thermal properties, as well as environmental compatibility. One of the most important parts of the program is the irradiation testing of both loose and bonded fuel samples. These samples, representing materials variables, processing variables, and production proof testing, are being irradiated in different capsules to partial and full fast-flux exposures.

Particle Fuel Systems

Blended Beds. As an alternative to bonded fuel rods, columns or beds of blended fissile and fertile fuel particles are being investigated. These would simplify manufacturing and eliminate the irradiation-induced stresses between fuel particles and matrix. Fairly high loadings of charcoal powder would be added to the beds to act as a sorbent for metallic fission products. Also, some means of restraining these beds may be required to prevent segregation during shipment. Two promising methods have been selected for testing: (1) a plug of low-density carbon or graphite felt that remains in place throughout core life but compresses to accommodate shrinkage of the graphite fuel block, and (2) a temporary binder, such as polystyrene or paraffin, to bond the top inch of the fuel bed to the graphite. Truck shipping tests are the principal method of evaluation, because the "white noise" vibration encountered on trucks is very difficult to duplicate in the laboratory.

The simultaneous metering technique of blending and loading fissile and fertile fuel particles into molds for rod fabrication can also be used for loading them directly into the graphite elements. This method gives a homogeneous blend over a wide range of particle ratios. However, initial attempts to add dry charcoal powder continuously to the stream of fuel particles during the blending and loading step were not successful. Because of the fluffy nature of the charcoal powder used, relatively low loadings of charcoal in the bed were obtained along with a nonhomogeneous distribution. However, when charcoal powder of a larger particle size (50 to 100 μm) was used, the material flowed in freely with the fuel particles giving a homogeneous bed. Charcoal loadings of up to 0.1 g/cm^3 were obtained with a reduction in fuel particle packing fraction of only 2% to 4%. Further work will be done with this method of charcoal addition.

Another method of charcoal addition that was tried consisted of passing a slurry of charcoal in acetone or water through the bed of fuel particles and building up a "filter-cake" of charcoal in the bed. Homogeneous loadings of 0.18 g of charcoal per cm^3 of bed (0.58 g/in. of length) were obtained on short beds 0.5 in. in diameter and 2 to 4 in. long. Difficulty was encountered, however, in applying this method to longer beds. In one initial attempt on a 30-in. column, it required 45 min to build up a 7-in.-long bed of charcoal.

Thermal cycling tests are being conducted on beds of TRISO and BISO fuel particles, both with and without charcoal powder. The beds (0.5 in. in diameter by 2 in. long) are contained in graphite crucibles. The samples are cycled between 240° and 850°C (with some additional cycling between 240° and 1250°C) using a period of 30 min per cycle. Some of the temperature differences across the beds were 70°C for the TRISO particles at 840° and 130°C for the BISO particles at 1010°C . The samples are being subjected to approximately 100 cycles in each test.

Thus far, no particle damage has been observed with the samples tested. This was determined by both burning (TRISO) and chemical analysis tests. Changes in bed height after 100 cycles appear to be about a 0.5% to 1% decrease. Additional testing is planned.

The thermal conductivity of several blended beds of fuel particles with charcoal was measured and the data are given in Table 3.1. In general, the values are approximately the same as for beds without charcoal.

Table 3.1
THERMAL CONDUCTIVITY OF BLENDED BEDS OF FUEL PARTICLES WITH
CHARCOAL AND GRAPHITE FLAKE ADDITIONS

Run No.	Specimen	Thermal Conductivity at 900°C (1650°F)	
		Cal/cm-sec-°C	Btu/ft-hr-°F
H 0305	BISO particles, 50 vol-% 4000-161, 50 vol-% 4000-159 + charcoal	0.0078	1.9
H 0306	BISO particles + 6353 natural-flake graphite	0.0107	2.6
H 0307	TRISO particles + charcoal	0.0111	2.7
H 0308	BISO particles, 50 vol-% 4000-159, 50 vol-% 4000-161 + charcoal	0.0103	2.5

Graphite Development

The fuel element and reflector graphites are being supplied by Great Lakes Carbon Corporation. The material, which is designated GLCC graphite grade H-327, is made from a calcined needle-type petroleum coke filler of 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 strength 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 2800°C (+300°, -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. As of July 2, 1969, 630 logs of H-327 grade graphite have been extruded and graphitized and are awaiting machining. All are acceptable as Grade A or B materials according to the specified gradings based on nuclear purity and mechanical strength given in Table 3.2.

Specimens from three grade H-327 graphite logs and of the total production quantity given in Table 3.2 have been mechanically tested to confirm the suitability of the material for use as components in irradiation capsules F-24, F-25, F-26, and F-27. More complete tests were also conducted to continue the surveillance of production materials in substantiation of the design parameters established on pilot-grade materials. The results of mechanical, density, and chemical analysis tests are presented in the following sections.

Table 3.2
STRENGTH AND NUCLEAR PURITY SPECIFICATIONS FOR
FUEL ELEMENT AND REFLECTOR GRAPHITE

	Grade A	Grade B	Grade C
	Fuel Elements	Replaceable Reflector Elements	Nonreplaceable Reflector Elements
Minimum tensile strength, psi	1,000 ^a	600 ^a	600
Maximum naturally occurring impurity levels, ppm: ^b			
Boron and boron equivalent (755 barns/atom)	5	5	10
Fe (2.4 barns/atom)	100	100	2,000
Ti (5.6 barns/atom)	50	50	100
V (5.1 barns/atom)	50	50	100
Total ash	1,000	1,000	10,000

^a-At center of log parallel to axis of extrusion.

^b-For Grade A, total thermal neutron absorption cross section for all elements not to exceed 5 ppm boron equivalent.

Tensile Tests. Room-temperature tensile tests (Tables 3.3 and 3.4) show a spread of 1400 to 723 psi in the ultimate tensile stress (UTS) in the weakest plane of 18-in.-diameter H-327 graphite logs. The weakest plane of the 18-in.-diameter, 31-in.-long extruded log is the cross section at the mid-length position (mid-length, transverse orientation). The weakest point within a log as indicated from the preliminary tests is shown to be the transverse orientation in the center of the log.

The UTS of H-327 graphite in the direction parallel to the extrusion direction compares favorably with EGCR materials ranging between 1140 and 2674 psi. (These values are 14% to 160% above the minimum specified value.)

The mean strains at fracture of H-327 graphites of 1.4×10^{-3} in./in. in the parallel orientation and 1.7×10^{-3} in./in. in the transverse orientation compare favorably with similar types of graphite, notably EGCR material.

A comparison of the modulus of elasticity of H-327 graphites with other similar materials is not possible without a full knowledge of the methods by which values are derived. The standard chosen for the PSC production materials is the chord modulus between stress values of 250 and 500 psi, in addition to the secant modulus (UTS/strain at fracture). The values for the chord modulus of production grade graphite logs ranges between a maximum of 2.5×10^6 psi in the parallel direction to a minimum of 0.5×10^6 psi at the middle of the log in the transverse orientation.

Table 3.3

TENSILE PROPERTIES OF PSC GRADE A H-327 GRAPHITE
(Log No. 42/1960, 12-1, 997C, Furnace No. 40)

Property	Sample Position							
	Parallel Orientation				Transverse Orientation			
	End Edge	Mid-Length Edge	Mid-Length Center	End Center	End Edge	Mid-Length Edge	Mid-Length Center	End Center
Ultimate tensile strength, psi:								
Maximum	2670	2520	1200	1320 ^a	1300	1020	830	1400
Minimum	2640	2240	1140	1270	1250	1000	723	1380
Mean	2655	2380	1170	1295	1275	1010	776	1390
Strain at fracture, 10 ⁻³ in./in.: ^b								
Maximum	2.6	1.95	2.0	1.0	2.7	1.75	1.7	3.1
Minimum	2.05	1.5	0.65	1.3	2.55	1.80	1.5	2.9
Mean	2.325	1.73	1.32	1.15	2.62	1.775	1.6	3.0
Secant modulus, 10 ⁶ psi: ^b								
Maximum	1.29	1.43	1.85	1.32	0.5	0.832	0.49	0.476
Minimum	1.025	1.29	1.085	0.98	0.48	0.684	0.482	0.45
Mean	1.157	1.36	1.467	1.15	0.49	0.758	0.486	0.463
Chord modulus, 10 ⁶ psi ^b , between 250 and 500 psi:								
Maximum	2.08	2.5	2.5	1.4	0.635	0.832	0.55	0.625
Minimum	1.66	1.56	1.13	1.25	0.55	0.72	0.5	0.625
Mean	1.88	2.03	1.82	1.325	0.593	0.776	0.525	0.625

^a-Vendor's test result, 1505 psi.

^b-B-3M Hi Mag Extensometer GL 1 in., diameter 0.5 in.

Table 3.4
SOME TENSILE PROPERTIES OF PSC GRADE A H-327 GRAPHITE

Property ^a	Sample Position			
	Parallel Orientation		Transverse Orientation	
	End Edge	End Center	End Edge	End Center
Log No. 42/2163, 13-1, 83/9976, Furnace No. 83				
Ultimate tensile strength, psi	2280	1995 ^b	1210	1257
Strain at fracture (10^{-3} in./in.) ^c	2.0	1.9	1.3	2.8
Secant modulus (10^6 psi)	1.14	1.054	0.931	0.449
Chord modulus (10^6 psi) between 250 and 500 psi	1.47	1.25	1.25	0.595
Log No. 83/9976, 6-1, 2580, Furnace No. 83				
Ultimate tensile strength, psi	1995	1716 ^d	1257	1067
Strain at fracture (10^{-3} in./in.) ^c	1.4	1.6	2.8	1.5
Secant modulus (10^6 psi) ^c	1.425	1.144	0.449	0.61
Chord modulus (10^6 psi) ^c between 250 and 500 psi	1.6	1.273	0.595	0.695

^a-Tests to establish material properties for capsule F-24, F-25, and F-26 materials.

^b-Vendor's test result, 1500 psi.

^c-B-3M Hi Mag Extensometer GL 1 in., diameter 0.5 in.

^d-Vendor's test result, 1680 psi.

Compressive Tests. The compressive strength of H-327 graphite (Table 3.5) shows little variation due to orientation within a production grade log. The minimum crushing strength of the material in the parallel plane is 7% to 15% lower than in the transverse orientation. The ultimate compressive strength within the production logs of graphite tested ranged from 3870 to 5015 psi. The stress-to-strain ratio under compression ($E_{\max}$) of H-327 graphite is related to the slenderness ratio (length/radius of gyration) of the test piece and is approximately doubled as this value is reduced one half, while the proof resilience of the material is reduced approximately fourfold.

The modulus of rupture (MOR) of rectangular beam sections of H-327 graphite (Table 3.6) ranges between 1957 and 3758 psi for the parallel orientation of the log and 1530 to 2307 psi for the transverse orientation. The UTS of H-327 graphite calculated from the MOR tests can be more than 30% greater than maximum values calculated from tensile tests. The lowest values measured

Table 3.5

COMPRESSIVE STRENGTH OF PSC GRADE A H-327 GRAPHITE
(Log Nos. 83/9976, 13-1, 42/2163, Furnace No. 83,
and Log No. 83/9976, 6-1, 2580, Furnace No. 83)

Property	Orientation						Specimen Length ^a (in.)
	Parallel			Transverse			
	Max	Min	Avg	Max	Min	Avg	
Compressive stress at fracture, psi	4684	3870	4413	4989	4531	4776	0.5
	4633	4124	4446	5015	4445	4683	1.0
Strain at fracture (10 ⁻³ in./in.)	40	30	33	54	44	52	0.5
	20	20	20	37	37	37	1.0
E _{max} compressive (10 ⁶ psi)	0.229	0.170	0.203	0.145	0.14	0.142	0.5
	0.575	0.288	0.401	0.233	0.232	0.236	1.0
Proof resilience (in.-lb/in. ³)	31	20	24.5	29	28	28.5	0.5
	7.5	6.3	7.0	9.5	6	7.5	1.0

^aAll specimens 0.5 in. in diameter.

within the production grade test specimens was 1777 psi. One possible explanation is the natural selection during machining of the test piece surfaces to eliminate gross defects. Internal defects have no effect on the strength of a bend specimen over a large proportion of the load range. This is clearly evidenced by a series of tests on 0.25-in.-thick parallel orientation beams bearing a 0.125-in. hole through the neutral axis position, since the range of MOR values of 1873 to 3570 psi with a mean value of 2721 psi is similar to the values measured on apparently homogeneous beams.

Fracture in H-327 Grade Graphite. The mode of fracture in bending beams 0.25 in. thick by 0.5 in. wide machined from H-327 graphite is shown in Fig. 3.1. These tests were conducted to show the significance of defect position in raising or lowering the apparent strength of a graphite in flexure. It is seen that fracture initiates at the position of maximum beam stress by separation within the filler particle at what were in all probability existing cracks or planes of weakness. Many researchers point out that graphitization tends to preferentially orient crystallites in filler particles. If comminution has initially accentuated acicularity by breaking up the original cokes along preferred cleavage planes, then the packing of particles takes a preferred orientation in a log. A crack propagating through the graphite along these paths is diverted according to the orientation of the filler, and separation first occurs on those planes of weakness normal to the principal stress direction. Coherency of the filler cracks to the point of failure of the graphite is related to the degree of orientation and length of the major axis of each filler particle.

The porosity in graphite that results from incomplete filling of the filler particle interstices is, from bulk density measurements, greatest in

Table 3.6

MODULUS OF RUPTURE OF BEAMS 0.17-IN. THICK SIMPLY SUPPORTED AND
LOADED WITH TWO-POINT SYMETRICALLY PLACED CENTRAL LOAD

Property	Position in Log	Mean Values					
		Parallel Orientation			Transverse Orientation		
		1-in. Wide	0.5 in. Wide	0.25 in. Wide	1-in. Wide	0.5-in. Wide	0.25-in. Wide
Outer fiber stress at fracture, psi	Center	3758	1957	2247	1575	2294	1530
	Edge	2265	2987	3512	2307	1777	1833
Deflection at fracture (10^{-3} in.)	Center	30.5	42.5	26	38	46	31
	Edge	27.5	28	37	35	38	38

Section through 0.25-in.-thick beam with 0.0159-in. hole at extreme fibers. Fracture stress 1722 psi (MOR). Deflection at failure 0.032 in./in. of span.

Continuing section through beam at original neutral axis.

Continuing section through beam initially in compression. Bearing load at this point of fracture preceding total failure, 23.5 lb on two-point central load on equidistant span of 2.5 in.

Fig. 3.1--The mode of fracture in bending beams 0.25 in. thick by 0.5 in. wide machined from H-327 graphite

the center of a graphite log. The mechanical strength is lowest at the center of a graphite log because of the reduction in area normal to any applied load. The large pores in H-327 graphite are mostly flat and therefore present a bigger reduction in area to an applied load on certain planes. The density of a log of H-327 graphite is shown to be a minimum at the center (Table 3.7). Accepting that differences in particle orientation may be a major contributing factor to differences in strength, it is also thought possible that the approximate twofold increases recently measured in the strength of H-327 logs at the outer fibers as compared to the center are related to differences in pitch impregnation. One interesting effect of porosity in H-327 graphites is that crack propagation is impeded at each pore site and initiation of fracture has to reoccur while the orientation and direction of the filler fracture plane may be different, thus effectively resolving the fracture force.

Coefficient of Thermal Expansion. The coefficient of thermal expansion (CTE) of production H-327 graphites is shown to be within the range of the reported values for the pilot production graphites in the parallel orientation (Table 3.8). However, the degree of anisotropy as indicated by CTEs is less in the production materials since initial tests show the transverse orientation has values approximately 20% lower than the measured CTEs of the pilot production grades. The principal contributing factor may well be the difference in diameters of the extruded logs. It is likely, therefore, that the dimensional changes due to irradiation as reported in the two preceding quarterly reports (GA-9261 and GA-9440) may represent minimum values for the transverse orientation and maximum values for the parallel orientation.

Chemical Purity. Samples from 12 logs out of the first ten production furnace batches of H-327 graphite have been spectrochemically analyzed. One sample from the outer fibers of a log at the end position was shown to have unacceptable levels of boron (Table 3.9). The rare-earth content (other than uranium) is listed in Table 3.10.

Graphitization Process. A literature survey and a number of tests to study the effects of changes in the specified graphitization temperatures (2800°, -100°, +300°C) indicate the following data.

The trends in irradiation dimensional change, purity, and thermal conductivities of PSC H-327 graphites would be deleteriously affected by reductions in the specified graphitization temperatures. The core heat content would be reduced, and a beneficial strain relaxation (or creep) of the core components at normal operating temperatures may possibly be enhanced if the degree of graphitization were reduced.

The mechanical anisotropy is suggested to be unchanged with treatment, although crystals develop structurally to give crystallite diameter increases of about 10% with possibly an accompanying reduction in the value of Young's modulus.

Measurement of the interlayer spacings on the H-327 pilot run and initial production run materials are indicative that graphitization temperatures approaching 3000°C have been attained on the samples studied.

Table 3.7

TYPICAL DENSITY AND PERMEABILITY OF H-327 GRAPHITE

Property	Sample Position							
	Parallel Orientation				Transverse Orientation			
	End Edge	Mid- Length Edge	Mid- Length Center	End Center	End Edge	Mid- Length Edge	Mid- Length Center	End Center
Bulk density, g/cm ³ , log No. 42/1960, 12-1, 9977C	1.8009	1.779	1.7376	1.765	1.7935	1.776	1.7479	1.77
Mercury density, g/cm ³ , log No. 42/1960, 12-1, 9977C:								
14.7 psig	1.81	1.81	1.88	1.86	1.78	1.79	1.89	---
750 psig	2.001	2.021	2.029	2.024	2.012	2.004	2.032	2.024
Helium density, g/cm ³	2.04	2.12	2.0	2.14	2.14	2.04	1.99	2.01
Helium permeability, log No. 83/9976, 42/2163:								
Avg. effective pore radius, cm	---	4.2x10 ⁻⁴	---	8.7x10 ⁻⁴	---	6.8x10 ⁻⁴	---	7.1x10 ⁻⁴
Effective No. of pores per cm ²	---	3.4x10 ³	---	2.2x10 ³	---	2.1x10 ³	---	2.1x10 ³
Permeability, 1 atm, cm ² /sec	---	4.2	---	3.4	---	1.11	---	1.24
Helium permeability, log No. 83/9976, 6-1:								
Avg. effective pore radius, cm	4.2x10 ⁻⁴	2.4x10 ⁻⁴	---	---	1.36x10 ⁻³	1.07x10 ⁻³	---	---
Effective No. of pores per cm ²	3.4x10 ⁴	1.3x10 ⁵	---	---	3.6x10 ³	4.0x10 ³	---	---
Permeability, 1 atm, cm ² /sec	4.9	2.1	---	---	2.6	2.7	---	---

Table 3.8

CTE VALUES FOR PILOT AND PRODUCTION GRADE H-327 GRAPHITES

Position in Log	Orientation	No. of Samples	Coefficient of Thermal Expansion (10 ⁻⁶ in./in.)		Ratio with Alternative Orientation
			Mean	Std. Dev.	
G-13 H-327 Graphite, 12-in. Diameter Log (Mean CTE 22° to 1000°C)					
Edge	Parallel	40	1.603	0.31	0.374
Center	Parallel	30	1.6	0.26	0.415
Edge	Transverse	20	4.18	0.2	2.6
Center	Transverse	20	3.847	0.4	2.41
PSC Production Graphite, 18-in. Diameter Log (Mean CTE 22° to 700°C) ^a					
Edge mid-length	Parallel	1	1.60	---	0.486
Edge end	Parallel	1	1.56	---	0.453
Center mid-length	Parallel	1	1.58	---	0.475
Center end	Parallel	1	1.59	---	0.512
Edge mid-length	Transverse	1	3.29	---	2.059
Edge end	Transverse	1	3.45	---	2.21
Center mid-length	Transverse	1	3.32	---	2.1
Center end	Transverse	1	3.11	---	1.96

^aLog No. 83/9976, 42/2163.

Table 3.9

SPECTROCHEMICAL ANALYSIS OF IMPURITY LEVELS IN 12 LOGS OF GRADE H-327 GRAPHITE

Log No.	Log Position	Element Concentration (ppm) ^a								Log Avg Density (g/cm ³)
		Al	B ^b	Cu	Fe	Mg	Si	Ti	Ash ^c	
694	End edge	1.0	ND0.5	---	---	2.0	<10.0	4.0	21	1.78
705	End edge	1.0	1.0	---	---	2.0	<10.0	8.0	38	1.77
739	End edge	1.0	2.0	---	---	2.0	10.0	10.0	55	1.78
744	End edge	4.0	2.0	---	2.0	6.0	20.0	8.0	77	1.78
769	End edge	1.0	1.0	1.0	1.0	4.0	20.0	10.0	55	1.75
956	End edge	4.0	1.0	1.0	2.0	4.0	20.0	10.0	472	1.77
958	End edge	---	1.0	---	2.0	1.0	20.0	20.0	317	1.80
1058	End edge	1.0	1.0	---	---	4.0	20.0	10.0	72	1.78
1959	End edge	6.0	---	1.0	10.0	10.0	80.0	10.0	77	1.77
2479	End edge	4.0	1.0	1.0	1.0	6.0	20.0	10.0	65	1.77
2482	End edge	2.0	1.0	---	2.0	6.0	20.0	10.0	62	1.78
42/1960	End edge	20.0	10.0	<1.0	2.0	2.0	<10.0	6.0	47	
40/9977C	End center	2.0	---	---	1.0	2.0	<10.0	6.0		1.77
12-1	Mid-len. edge	4.0	4.0	---	1.0	4.0	20.0	8.0		
	Mid-len. ctr.	1.0	---	---	1.0	4.0	10.0			

^aAccuracy - 50%.^bNot detectable below 0.5%.^cVendor's reported chemical analysis.

Table 3.10
 RARE EARTH CONTENT OF H-327 LOG NO. 83/9976 42/2163
 (Produced in First Ten Production Furnace Charges)

Element	Content (ppm)	Element	Content (ppm)
Ce	0.1	Tm	0.02
Pu	0.2	Yb	<0.02
Tb	0.2	Sc	0.02
Nd	0.2	Sm	<0.02
Ho	<0.04	Gd	0.02
Lu	0.04	Dy	0.2
Er	0.02	La	0.02

NOTE: Method was spectrographic analysis; accuracy was $\pm 30\%$.

Control Materials

Reserve Shutdown System. The specification for the boronated graphite balls for the reserve shutdown system was written and issued. Two types of ball will be used. The type A ball will be 7/16 in. $\pm$ 1/32 in. in diameter and will have a boron density of $0.32 \text{ g/cm}^3 \pm 0.03 \text{ g/cm}^3$. The type B balls will be 9/16 in. $\pm$ 1/32 in. in diameter and will have a boron density of $0.66 \text{ g/cm}^3 \pm 0.03 \text{ g/cm}^3$. The balls will be held in a hopper above the core during normal service. In the event of an emergency requiring reserve shutdown system, the type A balls can be released into the center 19 regions and the type B balls into the outer 18 regions of the core. The boron density of balls in the center regions was specified in order to ensure adequate thermal stability in the event of an accident condition resulting in extremely high temperatures. The size difference was specified in order to ensure proper identification of the two types during assembly of the system.

Behavior of Boronated Graphite at Elevated Temperatures. The investigation of the behavior of poison material at temperatures expected in a postulated loss-of-forced-circulation accident is continuing. Compacted boronated-graphite specimens have been investigated in a series of experiments, and the preliminary results were presented in the previous quarterly report (GA-9440). The specimens were either cylindrical, 1.6 cm in diameter by 3 cm long, or 1/4-in.-diameter spheres stacked on top of each other. The samples contained from 12 to 32 wt-% boron and were subjected to static compressive loads of 14 to 16 psi.

The conditions and results of the tests on the boronated graphite material are given in Table 3.11. All of the samples at the end of the anneals retained their original shape. Little or no slumping was observed; however, substantial boron losses by either vaporization or diffusion were observed in experiments

Table 3.11

BEHAVIOR OF BORONATED GRAPHITE SPECIMENS AT HIGH TEMPERATURE AND COMPRESSIVE LOAD CONDITIONS

Exp. No.	Sample Type	Initial B Content ^a (wt-%)	Anneal		Compressive Load (psi)	Final B Content, Top to Bottom (%)	Structural Integrity
			Time (hr)	Temp ^b (°C)			
3915-47	Cylinder	12	1 1 1	3000 3100 3200	14	5.1, 5.3, 5.4, 5.6	No change
3915-51	Cylinder	20	1 1 1 1/2	3000 3100 3200 3300	15	5.5, 9.2, 11.2, 13.2, 13.7	No change
3915-53	Cylinder	32	3	3000	15	27, 29, 29.5	5% slump
3915-64	Spheres (3)	30	8.5	2900	16	26.9, 27.2, 27.2	No change

^a-Measured values.^b-Temperatures are believed accurate to $\pm 50^{\circ}\text{C}$.

conducted at temperatures above 3000°C. Slight gravity flow was apparent in the three cylinders. Although the three spheres were stuck together, they too appeared essentially undamaged by the combination of compression and high temperature.

Irradiation Testing

Capsule P20. Capsule P20 is the second proof test of PSC TRISO coated particles. Variations in several coating parameters are being tested in this experiment to guide the optimization of mechanical stress relationships in the coating layers under high fast-neutron exposures. These parameters, which include PyC type (LTI versus HTI), PyC thickness, PyC density, SiC thickness, and TRISO geometry, were described in detail in an earlier quarterly report (GA-8725).

The experiment was designed to be tested to a fast-neutron fluence of 5 to 6×10^{21} n/cm² ($E > 0.18$ Mev) and to the full PSC fissile particle burnup of 20% FIMA. However, in light of the premature termination of the P21 experiment, the irradiation was extended to 8×10^{21} n/cm², with a resultant higher burnup. The samples were irradiated at initial design temperatures of both 1300° and 900°C, which are near the PSC maximum and average temperature, respectively. The temperatures were programmed to simulate a typical PSC core maximum-temperature region.

The capsule has completed its eight-cycle ETR irradiation in the I13SE core position and was discharged from the reactor in late August. The post-irradiation examination of the fuel particles irradiated in this capsule are now in progress.

Capsule P22. Capsule P22 is the third proof-test irradiation experiment of TRISO coated particles. Thirty samples of TRISO coated particles are being tested in P22. These are primarily fissile-size particles prepared in large-scale (production) equipment and are designed to evaluate the effects of PyC type, PyC density, and SiC thickness on stability at high fast-neutron exposures. Also included are laboratory-prepared TRISO and advanced-design SiC-coated fissile-size particles, as well as TRISO coated fertile-size particles. The coated particle samples being tested were described in a previous quarterly report (GA-9261).

The P22 experiment is designed to be irradiated to a fast fluence of 7 to 8×10^{21} n/cm² ($E > 0.18$ Mev), which is 85% to 100% of the PSC maximum dose. The fuel will achieve burnups of 22% and 8% FIMA for fissile and fertile particles, respectively. The design temperatures are 1300° (three cells), 1100°, and 900°C, with the high-temperature cells programmed to simulate a PSC maximum-temperature core region.

Capsule P22 is undergoing its seventh of seven to eight planned cycles in the ETR in the I13SW core position. The current estimated burnup for the fissile particles is 21% FIMA, and the fast-neutron fluence is estimated to have reached 6.4×10^{21} n/cm². The majority of the temperatures are operating near or slightly below design. The 1300°C cells have been programmed downward to 1100°C.

Capsule FR-1. The objectives of the FR-1 fuel rod irradiation experiment were to test fuel rods to a fast-neutron fluence of 4 to 5×10^{21} n/cm² at 1250° and 900°C with full PSC burnup of the fissile particles.

The capsule contained five 0.5-in.-diameter by 1-in.-long fuel rods. The rods were designed to test TRISO LTI and HTI fissile and fertile particles bonded in injection-molded matrices. One rod was irradiated at about 1050°C (design temperature 900°C), and the other four were irradiated at about 1175°C (design temperature 1250°C). Loose particle samples of the same fuel type used in each of the rods were also tested; however, they operated about 200° to 300°C lower than the corresponding fuel rods. The fast fluence was between 3.8 and 5.3×10^{21} n/cm², and the burnups were between 16% and 22% FIMA.

The fissile particles were coated in the laboratory, and the outer isotropic PyC coats were deposited from propane gas. The fertile particles were produced in production equipment, and the outer isotropic layer was deposited from an acetylene-propane gas mixture. The fuel rods were made by the injection-molding technique. The rods were encapsulated in H-327 graphite crucibles during the irradiation with an 8-mil diametral gap between the fuel rod and the container walls prior to the irradiation.

After the irradiation, all of the fuel rods were removed from the capsule and inspected. Both the graphite crucibles and the fuel rods were dimensioned to determine the irradiation-induced dimensional changes. Metallographic examinations were conducted on the five fuel rods as well as on the five batches of mixed loose particles. In addition, pieces of four of the five rods were burned in order to determine the number of failed silicon carbide coatings.

A number of observations were made as a result of the postirradiation examination, and these, along with a discussion of the results, are summarized below.

First, all five of the fuel rods were observed to be intact (see Figs. 3.2 through 3.6). Four of the rods were observed to be whole and were sufficiently strong to be handled and to allow the dimensions to be measured by means of a micrometer. The rod from cell 3 was damaged by fragments of the saw blade (which broke during cutting of the graphite crucible), and it began to crumble while being dimensioned. Four of the five fuel rods (cell 5 being the exception) were found to be stuck in the graphite crucible, and the crucibles had to be sawed open in order to remove the rods.

Second, all of the rods were determined to have expanded during the irradiation (see Table 3.12). The diameters of the H-327 graphite crucibles were found to have shrunk slightly; the shrinkage was greater opposite the loose particle thimbles than opposite the fuel rods (see Table 3.13).

Third, more than 75% of the outer PyC coatings of both fissile and fertile particles were broken in the cell 1 rod, which was irradiated at 1050°C to a fast dose of 5.0×10^{21} n/cm². More than 95% of the PyC layers were observed failed on particles in rods from cells 2 through 4; these rods were irradiated at 1175°C and received fast doses between 4.8 and 5.3×10^{21} n/cm². The rod in cell 5 received a lower fast-neutron dose (3.8×10^{21} n/cm²) and

(S247-3, 4)

(~6X)

Fig. 3.2--Postirradiation appearance of rod No. 01-1A-C018 (capsule FR-1, cell 1)

(S247-40, 41)

(~6X)

Fig. 3.3--Postirradiation appearance of rod No. 01-2A-C019 (capsule FR-1, cell 2)

(S247-51, 52)

(~6X)

Fig. 3.4--Condition of rod No. 01-3A-C020 (capsule FR-1, cell 3) after being removed from irradiation capsule; rod was damaged by broken saw fragments while being cut from graphite container

(S247-16, 17)

(~6X)

Fig. 3.5--Postirradiation appearance of rod No. 01-4A-C021 (capsule FR-1, cell 4)

(S247-73, 74)

(~6X)

Fig. 3.6--Postirradiation appearance of rod No. 01-5A-C022 (capsule FR-1, cell 5)

Table 3.12

DESCRIPTION AND POSTIRRADIATION EXAMINATION OF FUEL RODS TESTED IN CAPSULE FR-1
(Five-cycle ETR Irradiation)

Rod No.	Particle		Coating		Irradiation Conditions					Postirradiation Examination				Macro. Exam. Remarks ^g	
			Type ^b	Outer Isotropic		Fissile Burnup (% FIMA)	Temp (°C)			Fast Dose (10 ²¹ n/cm ²)	Dim. Change (%)		Met. Exam.(% Fail)		
	Density (g/cm ³)	BAF ^c		Avg	Max		EOL ^e	Length	Diameter		Fissile	Fertile ^f			
01-1A-C018	3923-87E 4000-711 ^b	Fissile Fertile	TRISO LTI	1.95 1.90	1.01 1.01	21	1100	1300 ⁱ	1300 ⁱ	5.0	+1.2	+1.4	74	94	Intact-fragile. Matrix and coating cracks visible Burn: 0.3% SiC coatings failed.
01-2A-C019	3923-149E 4000-712 ^b	Fissile Fertile	TRISO HTI	1.86 1.95	1.03 1.01	22	1200 ⁱ	1330 ⁱ	1100 ⁱ	5.3	+2.3	+1.6	100	100	Intact - fragile. Matrix and coating cracks visible - some particles debonded. Burn: 0.8% SiC coatings failed.
01-3A-C020	3923-149E 4000-712 ^b	Fissile Fertile	TRISO HTI	1.86 1.95	1.03 1.01	22	1140 ⁱ	1280	1070 ⁱ	5.3	(j)	(+1.2)	100	100	Partially damaged during disassembly - fragile. Matrix and coating cracks visible. Burn: 0.5% SiC coatings failed.
01-4A-C021	3923-87E 4000-711 ^b	Fissile Fertile	TRISO LTI	1.95 1.90	1.01 1.01	20	1120 ⁱ	1290	1030 ⁱ	4.8	+2.1	+1.6	65	100	Intact - fragile. Pitted-matrix and coating cracks visible, some debonding of particles. Burn: 0.4% SiC coatings failed.
01-5A-C022	3923-87E 4000-711 ^b	Fissile Fertile	TRISO LTI	1.95 1.90	1.01 1.01	16	1160	1300	1115	3.8	+1.6	+1.0	9	100	Intact - good condition. Matrix and coating cracks visible. Burn: not performed.

^aFissile particle kernels are (Th,U)C₂; fertile particle kernels are ThC₂.^bTRISO particles have a silicon carbide diffusion barrier coating. LTI denotes low-temperature isotropic outer pyrolytic carbon coating, and HTI denotes high-temperature isotropic outer pyrolytic carbon coating.^cBacon anisotropy factor (BAF).^dAll fuel rods are about 1 in. long and 0.5 in. in diameter.^eEnd-of-life temperature.^fCracked outer isotropic PyC coatings only.^gBurn: a piece of the rod was oxidized in order to determine the percent broken silicon carbide shells.^hCoated particles produced in production equipment.ⁱEstimated; thermocouple failed during the test.^jNot determined.

Table 3.13
 DIMENSIONAL CHANGES OF THE H-327 GRAPHITE CRUCIBLES
 DURING IRRADIATION IN CAPSULE FR-1

Cell No.	Fast Fluence (10^{21} n/cm ²)	Opposite Fuel Rod		Opposite Loose Particles	
		Avg Temp (°C)	$\Delta D/D_0$ (%)	Avg Temp (°C)	$\Delta D/D_0$ (%)
1	5.0	500	ND ^a	405	ND
2	5.3	680	-0.15	635	-0.45
3	5.3	600	-0.41	630	-0.95
4	4.8	575	-0.39	640	-0.90
5	3.8	670	-0.31	670	-0.85

^aNot determined.

also contained the greatest amount of filler graphite; only 9% of the PyC coatings were found to have broken on the fissile particles, although 100% of the PyC coatings on the fertile particles failed.

Figures 3.7 through 3.11 show representative photomicrographs of the five fuel rods. The matrix material appeared to be well bonded to the PyC coatings, and many of the coatings appeared to be broken and distorted from the effects of the matrix shrinkage.

Fourth, approximately 35% of the fuel particles in the metallographically mounted segment from cell 1 (lower-temperature cell) showed signs of a chemical interaction (see Figs. 3.12 and 3.13). Further investigation showed that the Inconel sleeve surrounding the thermocouple had melted and reacted with the silicon carbide coatings, which were exposed and left unprotected since the outer PyC coatings were cracked.

It was observed that the temperature of the fuel rod increased during the irradiation (possibly because of a decrease in the thermal conductivity), and had reached approximately 1200°C before the thermocouple failed. The thermocouple was Chromel-Alumel. It appears that the rod reached about 1300°C during the last cycle of irradiation causing the Inconel to melt (melting point of Inconel is about 1340°C).

Fifth, while many silicon carbide coatings were observed to be fractured in all of the metallographic segments, it appeared that this was the result of the grinding and polishing operation. Pieces of four rods (all of the rod from cell 5 was mounted for metallography) were burned to oxidize the graphite matrix and outer PyC coatings in order to determine the condition of the silicon carbide coatings. Less than 1% of the SiC layers were found to be broken and nearly all of these were from fertile particles.

Sixth, four of the five batches of the loose fissile particles survived the irradiation, but only one of the five fertile batches did. Representative

(M247-75)

(75X)

Fig. 3.7--Photomicrograph showing typical appearance of bonded fuel particles and matrix material of rod No. 01-1A-C018. The large particle with the outer PyC coating broken is a fertile particle; the two smaller particles are fissile particles. Note that one of the fissile particles apparently survived the irradiation while the outer PyC coating broke on the other

(M247-94)

(~75X)

Fig. 3.8--Photomicrograph showing a representative segment of rod No. 01-2A-C019. Note that all of the outer PyC coatings are broken on the fertile and fissile particles

(M247-129)

(~70X)

Fig. 3.9--Photomicrograph showing representative segment of rod No. 01-3A-C020. Note that the outer PyC coatings are broken on both the fertile and fissile particles

(M247-105)

(~70X)

(M247-82)

(~140X)

Fig. 3.10--The upper photomicrograph shows a representative segment of rod No. 01-4A-C021; the lower shows a broken portion of a PyC coating that appears to be distorted (straightened out) by the strongly adhering matrix material. Cracks in silicon carbide layer are believed to be polishing artifacts

(M247-114)

(~70X)

Fig. 3.11--Photomicrograph showing a representative segment of rod No. 01-5A-C022. Note that the two fissile particles (smaller particles) survived the irradiation, whereas all fertile particles had broken outer PyC coatings

(S247-97)

(6X)

Fig. 3.12--Photomicrograph of sectioned portion of graphite thermocouple sleeve that was inserted in the center of fuel rod 01-1A-C018 (cell 1). Note that fuel particles are adhering to surface of the graphite

(M247-87)

(~70X)

Fig. 3.13--Closer inspection of segment shown in Fig. 3.12 revealed that Inconel diffusion barrier (used to separate BeO thermocouple tube from graphite sleeve) melted, infiltrated the graphite, and reacted with the adjacent fuel particles

photomicrographs of the loose particle batches are shown in Figs. 3.14 through 3.18. The failures were in all cases failure of the outer isotropic coatings, and none of the loose particle samples had significant failures of the silicon carbide coatings (less than 1% cracked silicon carbide coatings).

Approximately 18% of the outer PyC coatings of the TRISO-II HTI fissile particles from cell 2 failed; the fertile particles tested along with these fissile particles had about 30% failure of the outer isotropic coatings. However, the same types of particles were tested with a significantly lower failure percentage in cell 3, under apparently nearly identical conditions. The HTI particles in both cells 2 and 3 were found to have a strong Maltese cross pattern in the PyC coatings, which indicates that the coatings had undergone some reorientation.

Approximately 3.5% and 5% of the LTI fertile particles from cells 1 and 4, respectively, had cracked isotropic coatings. The same particles were tested in cell 5 with 0.3% failure; however, the fast dose in this case was about 30% lower than for the other two samples (see Table 3.14).

Finally, it was observed that five to 50% of the buffer and inner isotropic layers were broken in the LTI fissile particles; a higher percent failure was observed in the particles irradiated in the rods than in the loose particle. This is possibly because the rods operated 200° to 300°C hotter than the loose particles. In a few cases (less than 1%) the kernels extruded into these cracks and reacted with the silicon carbide layer (see Fig. 3.19).

No failures of the buffers or inner isotropic layers of the HTI fissile particles occurred.

In analyzing the results, a plot of the dimensional changes as a function of fast-neutron dose was made for the fuel rods irradiated in capsule FR-1 as well as for those irradiated in capsules P23, FR-2, and FR-3 (see Fig. 3.20). Two discrete curves can be drawn through the data points, one showing shrinkage and the other expansion of the rods. If it is assumed that all of the rods should behave similarly during irradiation, it appears there is a discontinuity or abrupt change in the dimensional change characteristics of the rods. One interpretation of this result is that initially the shrinkage of the rod results mainly from the shrinkage of the matrix. Since the matrix adheres very well to the outer isotropic coating of the particles, further shrinkage of the matrix stresses the PyC coatings and apparently causes them to break off the particles. At this point (labeled the "breakaway region" in Fig. 3.20) the rods are believed to undergo significant expansion resulting from dimensional changes in the now unrestrained PyC coatings. The later segment of curve, the "region of uniform expansion," is considered to be representative of rods that have nearly all of the outer PyC coatings broken. This curve, which can be extrapolated back to zero, indicates that if all of the isotropic coatings were broken before the irradiation the rods would have expanded uniformly because of irradiation-induced dimensional changes with increasing fast-neutron dose along the dotted portion of the curve.

(M247-3)

(75X)

Fig. 3.14--Representative fuel particles from loose particles tested in cell 1 of FR-1. The two larger particles are fertile particles (4000-711); the smaller is a fissile particle (3923-87E)

(M247-16)

(A)

(100X)

(M247-17)

(B)

(100X)

Fig. 3.15--(A) Representative fuel particles from the loose particles tested in cell 2 of FR-1. Note that fertile particle (4000-712) and one of the fissile particles (3923-149E) have cracked outer PyC coatings. (B) View in polarized light; note strong Maltese cross pattern

(M247-32)

(75X)

Fig. 3.16--Representative fuel particles from loose particles tested in cell 3 of FR-1. One fertile particle (4000-712) and three fissile particles (3923-14E) are shown

(M247-44)

(75X)

Fig. 3.17--Representative fuel particles from loose particles tested in cell 4 of FR-1. Two fertile particles (7000-711) and parts of three fissile particles (3923-87E) are shown

(M247-58)

(125X)

Fig. 3.18--Representative fuel particles from the loose particles tested in cell 5 of FR-1; one fertile particle (4000-711) and two fissile particles (3923-87E) are shown

(M247-121)

(~225X)

Fig. 3.19--Photomicrograph of particle with cracked inner isotropic and buffer layers; fuel kernel has extruded into the crack and come in contact and reacted with the silicon carbide layer

Fig. 3.20--Fuel rod dimensional changes as a function of fast-neutron dose (data from capsules P23, FR-1, FR-2, and FR-3)

Table 3.14

DESCRIPTION AND POSTIRRADIATION EXAMINATION OF COATED PARTICLES TESTED IN CAPSULE FR-1
(Five-cycle ETR Irradiation)

Lot No.	Type ^a	Coating										Particle ^c			Burou ^d (% FIMA)	Irradiation Conditions				Postirradiation Examination		Remarks	
		Buffer		Isotropic		SiC		Isotropic		RAP ^b	Total Coating Thickness (um)					Temp (°C)		Fast Dose (10 ²¹ n/cm ²)	Micro Exam (% Fail)	Met ^e Exam (% Fail)			
		Thickness (um)	Density (g/cm ³)	Thickness (um)	Density (g/cm ³)	Thickness (um)	Density (g/cm ³)	Thickness (um)	Density (g/cm ³)			Cell No.	Avg.	Max.		EOL ^d							
3923-87E	TRISO LTI	49	1.26	23	(f)	20	3.17	30	1.95	1.01	122	(Th,U)C ₂	150-250	2.25	21	1	725	810	810	5.0	0	0.7	TRISO LTI series. Mixed with TRISO LTI fertile particles.
3923-87E	TRISO LTI	49	1.26	23	(f)	20	3.17	30	1.95	1.01	122	(Th,U)C ₂	150-250	2.25	20	4	885 ^g	1000	840 ^g	4.8	0	0.4	
3923-87E	TRISO LTI	49	1.26	23	(f)	20	3.17	30	1.95	1.01	122	(Th,U)C ₂	150-250	2.25	16	5	930 ^g	1000 ^g	930 ^g	3.8	0	0	
3923-149E	TRISO HTI	49	1.26	25	(f)	21	3.17	32	1.86	1.03	127	(Th,U)C ₂	150-250	2.25	22	2	900	1135	860	5.3	5	18	TRISO HTI series. Mixed with TRISO HTI fertile particles.
3923-149E	TRISO HTI	44	1.26	25	(f)	21	3.17	32	1.86	1.03	127	(Th,U)C ₂	150-250	2.25	22	3	890	1060	910	5.3	0.05	0.5	
4000-711 ^h	TRISO LTI	43	1.05	19	(f)	20	3.19	45	1.90	1.01	127	ThC ₂	300-500	=	5 ^g	1	725	810	960	5.0	0	3.5	TRISO LTI series. Mixed with TRISO LTI fissile particles.
4000-711 ^h	TRISO LTI	43	1.05	19	(f)	20	3.19	45	1.90	1.01	127	ThC ₂	300-500	=	5 ^g	4	885 ^g	1000	840 ^g	4.8	0.3	5	
4000-711 ^h	TRISO LTI	43	1.05	19	(f)	20	3.19	45	1.90	1.01	127	ThC ₂	300-500	=	4 ^g	5	930 ^g	1000 ^g	930 ^g	3.8	0	0.3	
4000-712 ^h	TRISO HTI	43	1.05	19	(f)	20	3.17	40	1.95	1.01	122	ThC ₂	300-500	=	5 ^g	2	900	1135	860	5.3	8	30	TRISO HTI series. Mixed with TRISO LTI fissile particles.
4000-712 ^h	TRISO HTI	43	1.05	19	(f)	20	3.17	40	1.95	1.01	122	ThC ₂	300-500	=	5 ^g	3	890	1060	910	5.3	0	2	

^aTRISO particles have a silicon carbide diffusion barrier coating. LTI denotes low-temperature isotropic outer pyrolytic carbon coating, and HTI denotes high-temperature isotropic pyrolytic carbon coating.

^bBacon anisotropy factor.

^cAll particles are spheroidal and greater than 90% of theoretical density.

^dEnd-of-life temperature.

^eCracked outer isotropic coatings only; silicon carbide coatings did not fail.

^fNot determined.

^gEstimated.

^hParticles produced in production equipment.

It is difficult to assess the performances of the different types of coated particles because of the many variables involved, but it appears that the LTI outer isotropic coatings of fissile particles are stronger, or at least more stable during irradiation, than similar HTI coatings. It appears from the metallographic examination of the PyC coatings in polarized light that the HTI coatings undergo more reorientation during irradiation than do the LTI coatings. A similar behavior has been observed with BISO HTI and LTI particles. This result indicates that at high fast fluences the LTI fissile particles should perform better than the HTI fissile particles.

Little difference was seen between the outer PyC coatings on the HTI and LTI fertile particles that were tested in the fuel rod, since they all failed. The loose particle test did indicate, however, that the LTI fertile particles were slightly more stable than the HTIs. The difference in the performance of the fissile and fertile particles can possibly be attributed to the difference in the deposition condition (i.e., the fissile particles were produced from propane gases whereas the fertile particles were produced from a mixture of acetylene and propane); however, it is not certain at this time that this is the predominant factor in the failures.

The results of the burning operation are very encouraging, since they suggest that the silicon carbide layers probably remained intact during irradiation under severe conditions. The fission-product release of these particles would therefore be expected to be very low.

Capsule F-24. The F-24 irradiation capsule was discharged from the GETR on July 10, 1969 after all the tantalum sheathed tungsten-rhenium thermocouples had failed and an activity increase in the lead tube was noted. Visual observation of a reactor water flow test just prior to the discharge of the capsule revealed that one of the aluminum plugs adjacent to the capsule had come free of the capsule mounting bracket and was impacting the bracket and capsule. Postirradiation examination revealed a crack in the secondary containment of the capsule in an area adjacent to a welded standoff at the top of the capsule. The most probable cause of the secondary containment failure was a mechanical loading of the standoff against the filler piece under conditions of vibration. The failure occurred as a crack in the secondary containment tube immediately adjacent to the welded standoff, propagating circumferentially around the tube.

In order to prevent a recurrence of this type of failure, steps are being taken by Gulf General Atomic to make capsule design changes and by the reactor operator in modification of the dummy capsule leads and in the capsule installation and clamping arrangement.

Capsules F-25 and F-26. Capsules F-25 and F-26 will be companion tests in the ETR to half and full exposure for a six-year core. The coated particles and fuel rods to be tested are expected to be representative of those to be selected for the PSC first core. Nine samples of coated particles and ten fuel rod samples will be irradiated in F-25 at 1200°C to 4×10^{21} n/cm²; five duplicate fuel rods will be tested at 900°C. Capsule F-26 will test fuel rods, loose beds, and loose particles to a fast fluence of 8×10^{21} n/cm².

The F-25 capsule was shipped to the ETR and has begun its irradiation in cycle 104. It is in its first of four scheduled cycles and is operating

satisfactorily. Capsule F-26 was delayed one cycle and is scheduled to begin its irradiation the following cycle, ETR cycle 105.

Capsule F-27. Capsule F-27 is the replacement for capsule F-24, which was removed from the GETR because of damage that occurred during its first cycle of irradiation (see above). A few changes from the material in F-24 have been made for the F-27 capsule. A number of test particles coated using propylene gas have been substituted for particles coated using propane, and nine loose beds will be tested. The basic design of the capsule is unchanged, with 22 rod or loose bed samples and nine loose particle batches scheduled to be tested.

The capsule began its irradiation in GETR cycle 110; it is designed to receive a fast fluence of 4×10^{21} n/cm².

Capsules F-28 and F-29. Capsules F-28 and F-29 are currently being designed to test PSC reference material in the GETR. Fuel irradiated in capsule F-28 and F-29 will be similar in nature to that tested in F-25, F-26, and F-27, but the samples will incorporate any modifications in design parameters deemed necessary as a result of the postirradiation examination of the P20 capsule.

Capsule F-28 is designed for a fast fluence of 4×10^{21} n/cm², and the results should be available prior to the beginning of production of the fuel for the first half of the PSC core. Capsule F-29 will test fuel out to the full PSC fast fluence of 8×10^{21} n/cm², and the results will be available before the onset of production of the second half of the PSC core.

Radiography of Irradiated Particles. Microradiographs have been made of 50 to 100 particles from each of 29 different batches of BISO and TRISO fuel particles irradiated in various capsule experiments. Microradiographs also were made of representative unirradiated particles (but not the same particle) of each of these 29 batches. The fuel kernel diameters were then measured from the microradiographs using a microscope with a calibrated scale in the eyepiece.

By comparing the kernel diameters in the irradiated and unirradiated condition, an estimate was made of the amount of swelling that occurred during the irradiation. A fairly large uncertainty is associated with each of the measurements because the kernel diameters of the individual batches (irradiated and unirradiated) normally had a spread of about 100 μ m. The standard deviations from the average kernel diameters were determined to be about ± 25 μ m, which is a variance of approximately $\pm 12.5\%$. This spread in fuel kernel diameters is a natural consequence of the production technique involved in making the material.

The data are compiled in Table 3.15, which gives the irradiation conditions and the percent kernel swelling. A plot of the swelling as a function of burnup for various irradiation temperature ranges is shown in Fig. 3.21. The data points are scattered, but there is a perceptible trend that shows that the amount of kernel swelling increases with increasing burnup and temperature. This, of course, is the expected result.

An Arrhenius plot was made of the data shown in Fig. 3.21 by plotting the amount of swelling per percent burnup versus $1/T(^{\circ}\text{K})$. An "activation

Table 3.15
RADIOGRAPHIC KERNEL SWELLING DATA

Sample No.	Capsule	Temp (°C)		Burnup (% FIMA)	Kernel Diameter		Kernel Swelling, $\Delta D/D_0$ (%)
		Avg.	EOL ^a		Unirrad., $\pm 25 \mu\text{m}$	Irrad., $\pm 25 \mu\text{m}$	
2814-149E	P15	1230	1120	23	175	205	17.0
2814-149E	P16	1310	1330	15	175	191	9.0
2814-149E	P13H	1250	1225	21	175	203	16.0
2814-151E	P15	1210	1090	23	178	204	14.5
2824-9E	P15	1230	1120	24	178	199	12.0
2824-27E	P15	1230	1120	24	176	201	14.0
3023-77E	P13E	1180	1275	8.5	203	223	10.0
3023-79E	P13E	1190	1300	8.6	207	228	10.0
3023-81E	P13E	1080	1275	7.9	210	233	11.0
3143-135E	P14	950	925	5.7	183	189	3.5
3143-135E	P14B	885	925	17	183	194	6.0
3222-71E	P14B	880	935	17	189	206	9.0
3222-71E	P13F	1210	1275	9	185	196	6.0
3222-113E	P13H	1250	1225	22	185	218	18.0
3325-117E	P13H	1250	1225	22	183	223	22.0
(3325-117E)	P13H	925	950	22	183	220	(20.0)
3332-101E	P13F	1275	1290	11	178	210	18.0
3332-101E	P13F	915	910	14	178	182	2.0
3363-89E	P16	1280	1340	14	184	211	14.5
3363-101E	P16	1260	1300	14	183	222	21.5
3363-103E	P16	1290	1310	14	183	221	21.0
3363-121E	P16	1300	1330	5.6	365	382	4.5
3552-49E	P16	1290	1340	4.7	370	383	3.5
3552-59E	P16	1270	1310	5.6	354	393	11.0
3583-67E	P13J	1280	1190	17	194	226	16.5
3592-45E	P13J	980	980	14	201	213	6.0
3592-47E	P13J	1180	1100	16	189	212	12.0
3592-53E	P13J	1290	1200	16	191	212	11.0
3592-73E	P13J	1280	1190	17	178	213	19.5

^aEnd of life.

Fig. 3.21--Fuel kernel swelling as a function of burnup and irradiation temperature

energy" of about 15 kcal/mole was derived from this plot. Using this value an empirical equation for the kernel swelling as a function of burnup and temperature was derived,

$$S = B \exp 5.1 \frac{T - 1480}{T},$$

where S is the diametral kernel swelling in percent, B is the burnup in percent FIMA, and T the temperature in degrees Kelvin.

The swelling of the kernel during irradiation could, if the buffer is too small, bring the kernel in close proximity to the outer coatings. Damage to these coatings is then a possibility by fission recoils or, in the case of silicon carbide layers, as a result of chemical interaction. (Evidence of these types of reactions have been observed in some P-capsule tests.) The data obtained for kernel swelling as a function of temperature and burnup will permit an approximation of the maximum swelling that may occur during the life of the fuel, and the buffer coatings can be designed accordingly.

Coating Density Measurements of Irradiated Particles. The outer isotropic coating densities have been measured on ten different batches of unirradiated particles using the sink-float technique. The results of these measurements were in most cases in very good agreement with the previously reported values. The outer isotropic coatings have been separated from the particles of four batches of irradiated particles. The radioactivity level, however, is quite high, and density measurements will have to be conducted in the hot cell instead of the low-level hot laboratory as had been planned. These measurements will be made when the work load in the hot cell permits.

Graphite Irradiations. The continued irradiation of H-327 graphite will be carried out in the F series fuel irradiation capsules instead of in capsule G14 as originally planned. The tests on H-327 graphite to full PSC fast-flux exposure have to date been at temperatures above the maximum graphite temperatures in PSC. Additional design data are desired at 650° and 760°C, and a graphite cell has been added in the low-flux region of capsules F-25 and F-26 to continue the irradiation of selected G-13 samples which have been previously irradiated at these temperatures. These capsules also contain a number of samples of current production graphite within the low-temperature cells. There are also piggyback samples of H-327, 0.105-in. in diameter by 2 to 3 in. long to assess the irradiation effects of varying anisotropy throughout a production log.

CHEMISTRY

The objectives of this subtask are to determine:

1. The release behavior of solid fission products that might be important plateout sources and gaseous fission products from the fuel particles being considered for use in the plant. Release behavior will be determined as a function of time, temperature, and irradiation by means of analysis and postirradiation annealing and in-TRIGA furnace tests on irradiated fuel particles. The data will be used

to determine the effects of such variables as particle diameter, coating thickness, and coating structure and as input for the calculation of fission-product release to the coolant circuit. The particles used in these tests will be irradiated under other tasks of this program or will be obtained from other sources.

2. The sorption and diffusion behavior of solid fission products in graphites, being considered for use in the plant, as a function of temperature and concentration by means of analysis and out-of-reactor chemical experiments on graphite samples doped with "fissium."
3. The deposition (sorption) behavior of metallic fission products on coolant circuit metals and the relative distribution of fission products between carbon dust and metal surfaces.
4. The extent and effects of oxidation attack on both fuel particles and fuel element graphite as the result of steam leakage into the coolant circuit.

Fission-product Release

Production batches of TRISO coated particles irradiated to a burnup of about 16% FIMA at 900° and 1300°C in the P21 capsule showed extensive failure of the outer PyC coatings but survival of the SiC coating as indicated by the metallographic examination discussed in the previous quarterly report (GA-9440). In confirmation, preliminary in-pile release data presented in the same report showed that the SiC layers remained essentially intact for all the particles with failed outer PyC layers. The final data given in Table 3.16 confirm the earlier results and indicate that less than 1% of the SiC layers failed. These data were derived from radiochemical determinations after separation of the fission products from neutron-activation products of impurities in the capsule graphite. This concludes the planned fission-product release work on the specimens irradiated in capsule P21.

Impurity Attack on Fuel Materials

Steam-graphite Reaction Rate Studies. Experiments to determine the rate of steam oxidation of H-327 graphite are presently being performed. The objective is to provide accurate input data for the OXIDE computer code, which is used to calculate the over-all effects of steam ingress in HTGR systems.

The experimental procedure is to expose thin slabs of graphite to a flowing steam-helium mixture at temperatures from 750° to 1000°C in a recording microbalance. The weight loss of the sample versus time is thus automatically recorded. Although the reaction of steam with H-327 graphite is of prime interest, two other graphites, AGOT and TS-688, will also be investigated for comparison purposes.

It was anticipated that sufficient data could be obtained from the oxidation of two or three samples of each kind of graphite. However, some anomalous behavior was observed on two of four samples, and additional work is needed.

Reaction of Water with Fuel Material. Experiments to test the expansion of fuel particle beds because of reaction of water with the fuel materials are under way. This work is necessary to determine the fraction of broken

Table 3.16

IN-PILE RELEASE DATA FOR SELECTED P21 CAPSULE PARTICLES WITH FAILED PyC LAYERS

Lot No.	Irradiation		Irrad. Temp (°C)	Fractional Release to Thimble			
	Cell	Thimble		Cs ¹³⁷	Ce ¹⁴⁴	Ba ¹⁴⁰	Sr ⁸⁹
4000-886	1	2	900	9.6×10^{-5}	4.4×10^{-6}	$< 5 \times 10^{-5}$	1.1×10^{-4}
4000-886	2	2	1100-1300	5.6×10^{-4}	1.8×10^{-4}	1×10^{-4}	4.2×10^{-4}
4000-889	1	3	900	1.0×10^{-5}	3.5×10^{-6}	$< 5 \times 10^{-5}$	1.2×10^{-4}
4000-889	3	3	1100-1300	5.7×10^{-4}	3.0×10^{-4}	6.5×10^{-4}	2.4×10^{-3}
3923-95E	4	2	1100-1300	5.6×10^{-4}	3.2×10^{-4}	4.5×10^{-4}	3.3×10^{-3}
3923-113E	5	2	1100-1300	$< 1.8 \times 10^{-5}$	1.2×10^{-5}	1×10^{-5}	9.6×10^{-6}

particles that can be tolerated in the reactor. Three steam exposures have been performed using BISO particles with failed fractions of 2%, 10%, and 50% at 250°C. Total bed expansion for the 2% failed batch was essentially immeasurable (i.e., less than 1%), whereas the 10% and 50% failed batches expanded 11.8% and 55%, respectively. Tests planned for the future include determination of the effect of charcoal in the bed and measurements of the stress applied to the restraining graphite structures as a result of fuel bed expansion.

Task IV

FUEL ELEMENT PROOF TESTING

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

The Fort St. Vrain proof test element continued to perform well during the quarter. The Chromel-Alumel thermocouple typically read 1650°F when the reactor was operated at full power. As of September 30, the element had generated a total energy of 783 Mw-hr.

PART II

PRIVATELY SPONSORED RESEARCH AND DEVELOPMENT PROGRAM

(Not the Subject of AEC Reimbursement)

Task I

FUEL TRANSFER MACHINE

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

The checkout tests of the fuel handling machine in air at room temperature continued during the quarter. The development of the manual mode of operation of the machine as controlled from the console was initiated. Each of the four basic motions of the machine, Z (vertical), R (index), θ (azimuthal), and ϕ (head rotation), were operated and the servo loops closed. The R and θ drives were developed completely, and after initial problems with the ϕ drive, it now operates satisfactorily.

Two areas requiring corrections were encountered in this part of the program. These were difficulties in the vertical drive system and in operating the machine within the fuel storage rack.

Initial testing of the Z-drive system showed the machine to operate roughly and erratically, especially at the higher vertical speeds. Maximum machine velocity was limited to 7 in./sec. Also, during this period failures occurred in several individual components, such as the hydraulic pump yoke position potentiometer and the signal conditioner for the hydraulic pressure transducer.

Investigations of the Z-drive system revealed that one major cause of the rough operation was electrical noise from the command feedback potentiometers. Contamination on the film of these potentiometers was causing the electrical noise, and after cleaning them considerable improvement in machine operation occurred.

The motion of the machine, however, was still not entirely satisfactory and further investigation proceeded. A mathematical model of the machine was developed, and studies were made on the 1108 digital computer. Results of these studies showed that operational disturbing frequencies were close to the natural frequency of the machine. This natural frequency in combination with the remaining noise from the potentiometers was causing some rough and erratic motion.

From the over-all analysis of the problem it was concluded that new hydraulic motors with a higher torque capacity would be required to improve the system to a satisfactory level. The motors would increase the system natural frequency with a corresponding decrease in the amplitude of the disturbing oscillations. The new motors have been ordered and will be installed during the next period.

When the machine was operated within the fuel storage rack, it was discovered that one of the guide rails was misaligned and caused the machine to bind. Also, there were interferences between the simulated fuel elements and portions of the rack structure.

An analysis of the rail that was misaligned showed that this rail could be permanently removed. There were a total of seven locations in the rack for rails, and with the elimination of one of these rails, the other six would guide the machine adequately and provide sufficient accuracy throughout the machine stroke. This rail was removed and the machine now operates without any binding.

Interferences with the rail structure and simulated fuel elements were in two general locations. These were in the areas of the rack seismic restraint rails and the vertical bulkheads that make up the rack walls. Analysis showed that the restraint rails could be permanently removed. Sufficient support of the column of elements would be provided by other portions of the rack, even though clearances were slightly enlarged. These rails were removed, and the interference was eliminated.

The other general area of interference was with the rack vertical walls. It was discovered that there was excessive weld buildup on some of the joints joining the wall plates together. These weld beads were ground down and the interferences were eliminated.

One other problem with the rack was an interference with the machine radius arm and a support structure for a rail. Local grinding of the rail supports eliminated this interference condition.

The development of automatic operation of the machine by computer control was then initiated. All of the basic programming was prepared, and some automatic operation begun. Individual motion of the Z, R, and θ drives were successfully demonstrated. A failure of a component in the control console that had to be returned to the vendor for repair delayed any more complex automatic operation. This component has now been returned and reinstalled into the console. After its checkout the automatic operation and development will be resumed.

An investigation is in progress to analyze the problems associated with the grappling of fuel elements that have been distorted into a bowed configuration. An informal test has shown that an element cannot be grappled by present techniques when it is tilted to simulate the maximum predicted bow. The investigation will determine what changes in the grapple head or the grappling technique will permit these bowed elements to be handled.

A study of the seismic responses of the fuel transfer machine when suspended in the core was completed. Results of this study showed that no damage will result from exposure to a "single" or "design" earthquake. For a "double" earthquake condition, a resonant frequency condition could exist when the machine is stroked deeply into the core. The stresses in the machine telescoping tubes during a seismic excursion would then be beyond their yield points. This may be acceptable and is under further evaluation.

Task II

SERIES-STEAM-TURBINE-DRIVEN HELIUM CIRCULATOR

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

PROTOTYPE CIRCULATOR TESTING

Strain Gage Tests

The blade vibration tests on the helium circulator were completed at the Valmont Test Facility. Strain gages were mounted with instrument spot welds on the rotor blades of both the helium compressor and the steam turbine at the point of maximum strain. A rotary transformer was used to transmit the dynamic strain signals from the rotating to the stationary parts of the system. Bridge circuits arranged in a matrix array to minimize the number of rotary transformer coils provided four output channels each capable of monitoring seven gage signals. Each selected set of signals could be visually displayed on an oscilloscope, a Panoramic Analyzer and a Spectral Dynamics spectrum analyzer and also was simultaneously and continuously recorded permanently on tape for later evaluation. Because of assembly and checkout difficulties, six steam turbine gages and seven compressor gages could not be used during the tests. However, sufficient information was recorded from the remaining gages, since considerable redundancy had been incorporated.

It was not possible to cover all the planned test points because full steam flow was not available initially. Later, when sufficient steam was available, an indication of inadequate cooling within the strain gage system coupled with a loss of proper signal output caused termination of the testing as 9300 rpm was being attained. Leakage of hot helium into the strain gage cooling water system was indicated.

Table II.1 summarizes the tests that were completed. Following circulator shutdown, a detailed analysis was made of the stall data. Results from the stall tests are plotted on Fig. II.1, which shows the relation between the measured compressor blade alternating stress and the circulator rotational speed. As indicated, the stress is proportional to the 2.62 power of speed. On this basis, the alternating blade stress at stall conditions would extrapolate to 30,000 psi at the design speed of 9550 rpm, which is satisfactory and well below yield. It should be noted that the alternating stresses measured during conventional operation were much smaller than those observed during compressor stall.

Fig. II.1--Compressor stall tests, alternating blade stress versus rotational speed

Table II.1
MARK II HELIUM CIRCULATOR BLADE VIBRATION TESTS

Speed (rpm)	Restrictor Valve Setting (%)	Strain Gage Excitation Channel	Tape No.
Ramp from:			
3000-7900	Open	7	8
3000-7600	17.5	3,7	6,10
3600-7400	36.5 ^a	5	4
3000-7700	58.0	5,3	14,15
Resonance search at:			
4500	17.5	3	6
5000	17.5	3	6
5500	36.5 ^a	5	4
6400	58.0	5	14
7600	17.5	7	10
3000 (stall)	62.5	5	12
3000 (stall)	Closed	5	12
5000 (stall)	62.5	5	13
5000 (stall)	Closed	5	13
7000 (stall)	62.5	5	13

^a-Simulates various reactor operating points; 36.5% closed represents system design point with all four circulators operative.

In general the turbine blade stresses were much lower than those encountered by the compressor. The turbine blade resonance occurred in the 6000 to 7000 rpm speed zone as predicted.

Steam Turbine Tests

All of the circulator aerodynamic data from the steam tests reported in the previous quarterly progress report (GA-9440) have been reduced by computer. Figure II.2 confirms the preliminary conclusion that the performance of the compressor was better than predicted. It was not possible to determine the compressor efficiency in this test series. The efficiency was to be determined by measuring the water flow and the temperature rise across the heat exchanger that extracts the heat of compression from the helium. The product of the water flow and temperature rise in appropriate units would give the actual compressor power. Unfortunately, when the machine was dismantled, the flowmeter for the water was found to be entangled with weeds from the cooling pond. Further attempts at calculating the efficiency will be made during the acceptance test of the production circulator.

Fig. II.2--Compressor performance

The turbine data appear to have a crossover point between predicted and experimental performance (see Fig. II.3). At the higher speeds, and choked nozzle conditions, the pressure ratio (or work) was greater than predicted. For lower speeds, the opposite was true. It should be noted, however, that none of the data were obtained near the design point for each of the speeds as depicted by the solid symbols in Fig. II.3. The steam at Valmont is at a lower pressure than in the plant, and as such the turbine always operates at a part-load condition. If the helium pressure is reduced substantially the design point can be approached.

Fig. II.3--Steam turbine performance

Task III
CONTROL ROD DRIVE

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

The last portion of the control rod drive test program, the core misalignment tests, was completed without incident, except that scram No. 1555* was unsuccessful. At the conclusion of the testing, the control rod drive was disassembled and examined; all components were found to be in excellent condition with no evidence of wear or deterioration except in the case of the motor brake. Here, some wear was apparent, and it was concluded that this was the cause of the unsuccessful scram during the misalignment tests. This wear probably also caused the unsuccessful scram that occurred during the Phase II tests (see the previous quarterly report, GA-9440).

On both occasions, no rod movement occurred when the scram signal was initiated (i.e., when holding current was removed from the motor brake). Tests were conducted to ascertain that there was, in fact, no voltage at the brake coil. The brake plates appeared to have separated when observed through the viewing port in the autoclave. This port, however, affords only an oblique sighting to one side of the brake, and the plates could actually have been skewed, particularly as the separation between them is normally only 1/64 in.

In the first case, rod movement was initiated by jogging the shim motor once only in a "rod in" direction. A normal scram resulted. In the second case, the brake was reset (reenergized). When a scram was again initiated rod motion occurred immediately and the scram characteristics were normal.

Disassembly upon conclusion of the drive test program (at which time a total of 1554 scrams had been performed) showed the motor and brake assembly to be in a free-running condition identical to that at the start of test. There appeared to be no tight spot or restraint that would prevent the rods from falling under gravitational force. A similar condition existed throughout the rest of the mechanism.

The brake unit, shown in Fig. III.1, consists basically of two friction disks loosely mounted on an involute spline member attached to the motor end bell. The disks are thus free to move axially but are fixed rotationally. A third disk, sandwiched between the two friction disks, is keyed to the motor

* Scram count includes those scrams previously accomplished during the Phase II tests.

Fig. III.1--Diagram of brake unit

shaft by a spider member. It is free to move axially but rotates with the motor shaft. A hole is provided in this disk to fit over the involute spline with a nominal radial clearance of 0.031 in.

Inspection revealed a significant wear path around the tops of the involute splines (Fig. III.2) that was obviously caused by rubbing of the center disk. The wear was sufficient to throw up burrs on the faces of the spline teeth and thus impede free axial movement of the friction disks. Figures III.3 and III.4 show a typical friction disk and the center disk after the test.

An over-all dimensional check of the assembly showed that it was possible for the center disk to move radially enough to reduce the 0.031-in. clearance to the point of interference. The main cause of this was a 0.02-in. runout between the motor bearing housing and the register for the splined member. When this is coupled with a 0.003-in. runout between the register and the splines and the radial movement of the center disk permitted by its keys, the interference can be accounted for.

In view of the general free-running condition of the entire system, constraint sufficient to prevent the rods from falling could only have occurred within the brake unit and would have been of an intermittent nature.

The burrs on the spline teeth produced by abrasion of the center disk prevented free movement of the friction disks when the brake was deenergized. This caused the disk (or disks) to skew sufficiently to result in a hangup on the center disk.

This conclusion is certainly valid for the second unsuccessful scram, because resetting and releasing the brake corrected the condition. It is also the most probable cause of the first such occurrence, where, unfortunately, the same reset/release procedure was not attempted.

The applicable motor end bell drawing has been changed to require the splined member register to be concentric with the bearing housing within 0.001 in. TIR. In addition, the diameter of the hole in the center disk has been changed to increase the 0.031-in. radial clearance discussed above to 0.048 in. This action ensures that even under the worst case of tolerance stackup, including relative radial movement of the various components, the radial clearance will never be less than 0.025 in.

Fig. III.2--Closeup of involute spline member showing wear caused by center disk (HT-74557)

Fig. III.3--Typical friction disk after test (HT-74556)

Fig. III.4--Center disk after test (HT-74546)

Task IV
STEAM GENERATOR

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

STEAM-WATER FLOW STABILITY PROGRAM

Additional testing on the boiling stability test loop is under way in two areas. The first is a determination of static and dynamic stability of the loop representing steam generator modules performing between ring headers. This test mode represents behavior of a PSC hot module (exclusive of control system effects) reflecting plant gas imbalance which results in a nearly full-open feedwater trim valve. The second is a reinvestigation of apparent unstable operating points in the recently completed study, to determine whether interactions between the test coil and loop components are present that might lead to false indications of instability (such as occurred in tests simulating the 25% load condition). This additional testing is intended to support studies on the steam generator control system.

Previous data from the loop have indicated that a low margin in terms of static stability exists at reduced-load operation, especially for a hot module subjected to gas imbalance.

Some experimental studies by others on the control system stability of a once-through-type, two-tube boiler have shown that the concept employing steam temperature control by feedwater trimming displayed critical requirements for gain and reset adjustments at higher loads. At loads below 20%, stable operation could not be obtained.

A solution to a possible control system stability problem would involve a programmed increase in the pressure differential across the feedwater trim valve with load reduction. This would require the addition of an automatic function generator to the control system to execute the load/ ΔP program. This control system change could be accomplished in the field if startup data indicate the need.

A system stability analysis previously conducted was based on a linearized model of the DMOR code, which does not incorporate the hydraulic behavior of

the working medium. Therefore, this analysis is inadequate to assess the stability characteristics of the six parallel modules within a steam generator. To obtain adequate stability data, the following work has been initiated:

1. Hydraulics capability will be incorporated in the DMOR code and existing boiling stability test loop data will be evaluated.
2. Additional tests will be performed with the loop to obtain input data for the expanded DMOR code, which models the behavior of a steam generator module between inlet and outlet headers.
3. The DMOR code will be further expanded to encompass six steam generator modules in parallel and incorporate the results of item 2 above.
4. An analysis of an entire steam generator feedwater trim system will be performed to obtain valid stability data and control setting requirements.

MODULE AIR FLOW TEST

Work on the site for the module air-flow test continued during the quarter. The assembly of the water system is in progress. The air blower and motor have been assembled and checked out.

MATERIAL TESTS

Stress Rupture Tests on Sanicro 31

The test program currently being run to examine the effects of temperature transients on the stress-rupture life of Sanicro 31 solution annealed tube material, in the as-received condition and after varying degrees of cold work is now 75% complete. The results in general so far appear to be satisfactory.

Submerged-arc Welding Development

Work has been completed on the procedure for joining the 2-in.-thick standpipe (Incoloy 800) to the reheater (Incoloy 800) by the submerged-arc process. A weld was made in a piece of 1-in.-thick Incoloy 800 without difficulty. The results of a metallurgical examination and mechanical testing have shown this to be a most satisfactory welding process for thick welds in Incoloy 800.

Fatigue Testing

Continuous low-cycle fatigue testing of Incoloy 800 has been completed by the General Electric laboratory at Evendale, Ohio. Evaluation of the data resulted in the conclusion that the preliminary Incoloy 800 design curves, proposed from stainless steel data, are adequate for strain rates of 4×10^{-4} sec⁻¹ within the low-endurance range. Yet to be completed are a limited number of tests to investigate the effects of hold periods on the material's fatigue

properties. Preliminary tests have been initiated at Gulf General Atomic to determine areas to spot-check for this data. The tests will be conducted at Battelle Memorial Institute.

TUBE PLUGGING MACHINE

The major design effort during this period was concentrated on the construction drawings for the tube plugging tools. The construction drawings for the steam generator concentric liner handling tools and other installation equipment were also prepared during this period.

The test program for the seal-weld tools was initiated during the quarter. Figure IV.1 shows the seal-weld tools installed on the turret and drive assembly. The operation of the centering roller system, the viewing system, and the vertical position sensing system were evaluated prior to the start of the seal-weld tests.

The seal weld parameters were developed on inexpensive rings rolled from Inconel 600 bar stock. The final qualification tests were performed on samples designed to simulate the omega seal in the steam generator. The edges of the qualification weld joints were buttered in Inconel 82 filler metal, which was machined to provide a wall thickness of 0.125 in.

Two weld samples were placed in the test dome to make a horizontal joint with an inside diameter of 13-7/8 in. The welding process was tungsten-inert gas. The joints were fused together without additional filler metal.

The electrode was made of 2% thoriated tungsten and was 3/32 in. in diameter. The electrode was sharpened to a pencil point and was positioned to give a cold gap of 0.045 in.

The torch assembly was a standard commercial torch rated for 200-amp service. The torch contained a gas lens in a size 7 ceramic cup. The inside diameter of the cup was 3/8 in., and the electrode was positioned to extend 5/32 in. beyond the cup. The torch was mounted in a horizontal plane and was inclined 15° ahead of the radial line from the tip of the electrode to the center of the tool assembly.

The shield gas to the torch was argon at a flow rate of 30 cfh. The sequence programmer automatically provided a preweld purge of approximately 30-sec duration and a postweld flow of approximately 25-sec duration. An argon flow of 8 cfh was routed to the back side of the seal weld under the manual control of the machine operator.

The arc was initiated automatically at the end of the argon purge by superimposing a high-frequency alternating current on the normal straight polarity direct current. The initial attempts to start the arc were unsuccessful when the high-frequency unit was mounted in the welder with a weld cable length of approximately 90 ft. The high-frequency circuit was relocated to a position approximately 20 ft from the electrode and functioned normally in subsequent weld tests. The weld current immediately after the formation

Fig. IV.1--Two views of seal-weld tools installed on turret and drive assembly

of the arc was approximately 50 amp. The sequence programmer controlled the rate of increase in current until the normal weld current of 142 amp was attained.

The azimuth stepping rate was adjusted to provide a welding speed of 4 in./min. The start of azimuth movement was delayed for a few seconds by the sequence programmer to allow the weld puddle to form.

After the electrode had moved completely around the weld joint and had passed the initial starting point by approximately 1-1/2 in., the weld termination sequence was initiated by the operator. The termination sequence is controlled by the sequence timer and includes a gradual reduction in weld current from the normal value to about 50 amp. When the current reaches this value the arc is terminated and the azimuth motion stopped. The postweld argon flow continues for approximately 25 sec.

Figure IV.2 is a photograph of a typical segment of seal weld as it appears on the viewing system monitor.

Six consecutive qualification welds were made successfully to satisfy the test requirements. Specimen Nos. 1, 3, and 6 were sectioned for metallurgical examination. All specimens were dye-penetrant inspected and helium leak-checked. The leak rate through the welds was the same as the background reading on the mass spectrometer, and the detector reading for a calibrated leak (600 micron-ft³/hr at 1 atm differential) was greater by a factor of approximately 1000. Three of the six welds were sectioned for metallurgical examination.

Fig. IV.2--Typical segment of seal weld as it appears on the viewing system monitor

Task V

COATED PARTICLE DEVELOPMENT

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

COATING DEVELOPMENT

One of the problems associated with the fluidized bed coating process is the accurate control of the coating temperature during the initial stages of the coating operation. When laboratory-scale coaters are used, the built-in rapid thermal response is adequate to smooth out temperature discontinuities. However, in large coaters, heat inflow is slowed down because of the larger chamber diameters and higher bed densities (slower circulation). With a gas such as propane (C_3H_8), with its highly endothermic characteristics, temperature control is very difficult at all stages of coating and is particularly difficult when applying thin coatings. For these reasons, propylene (C_3H_6), a gas with similar coating characteristics but lower heat requirements, was evaluated as a replacement for propane. Propylene has been used by ORNL for applying LTI coatings for several years, and has been recommended by ORNL as a replacement for propane for the reasons given above.

Laboratory evaluation of propylene has been carried out over the temperature range of 1100° to 2000°C. The data obtained from these experiments indicate that the prime objective of better temperature control can be met without any important differences in the coatings compared with those produced from propane. When critical coating properties, such as density and preferred orientation, are plotted versus temperature, both propane- and propylene-derived coatings fall on the same curves between 1000° to 1500°C. This is shown in Figs V.1 and V.2. The crystallite size obtained with the two different coating gases is also identical over the same temperature range.

The microstructural appearance of the two coatings is identical, with no differences noted at magnifications below about 400X. Metallographic analysis at magnifications greater than 400X has not been made. The microstructure of the propylene coatings changes as the deposition temperature is increased, the structure showing increasing sootiness with higher deposition temperatures. At temperatures above 1550°C the soot exists as continuous laminations in the coating. There is very little interest in coatings deposited above 1550°C however, because the same physical properties can be obtained at lower deposition temperatures.

Fig. V.1--Plot showing the variation of LTI PyC density as a function of deposition temperature for propane- and propylene-derived coatings; data indicate that coatings are probably very similar

Fig. V.2--Plot of preferred orientation versus deposition temperature for propane and propylene coatings showing the close similarity of the coatings

These experiments with propylene as an LTI coating gas have shown that improved deposition conditions are achieved at no change in the important physical properties of coatings over those obtained by the use of propane. Irradiation tests of propylene-derived coatings are now in progress.

COATED PARTICLE EVALUATION

Characterization of LTI Coatings

Porosity of Propylene-derived LTI Coatings. In the evaluation of particle batches for the F-24, F-25, and F-26 capsules, two tests were used to indicate porosity in outer PyC coatings: (1) Hg porosimetry and (2) BET measurement (by N_2 adsorption). In general, porosimetry is conducted on 2- to 3-g samples, and the volume of mercury displaced is noted as a function of the pressure applied. The curves obtained are generally similar to Fig. V.3, and the "critical pressure" shown gives the size of the largest pores accessible to the mercury. In BET determinations, 15 to 25 g of particles are cooled down to liquid-nitrogen temperature and nitrogen is adsorbed onto their surfaces. The nitrogen is then desorbed at boiling water temperature, generally over a period of 2 to 3 min.

For LTI coatings from propane the critical pressure is between 500 and 700 psia, equivalent to a maximum accessible pore size between about 0.5 and 0.3 μm . Typical BET values are in the range 0.2 to 0.3 m^2/g for fissile particles, and 0.1 to 0.25 m^2/g for fertile particles. Recently, however, two batches of propylene-derived material have both given high specific surface area values as indicated in Table V.1 where the values for the equivalent propane batches made at the same time are also given. In addition, examination of the porosimeter curves also indicated high critical pressures in the region of 1000 to 1500 psia, for the propylene-prepared batches, equivalent to maximum pore sizes of about 0.26 to 0.17 μm . These data therefore suggest a significant difference in the porosity characteristics of propane and propylene coatings derived at the same temperature (the propylene coatings showing apparently larger surface areas and finer porosity).

The only significant difference previously noted in deposition characteristics when using propylene versus propane was a shift for propylene of 50° to 60°C towards lower temperatures in the curve for deposition rate versus average bed temperature. This was attributed to the lesser thermal energy required to decompose propylene compared with propane. It was considered possible that this small temperature difference could account for the porosity differences observed between the coatings, and the particles made in the parametric study were therefore subjected to BET determinations. The results are shown in Table V.2 and Fig. V.4.

It is obvious that the specific surface area is strongly temperature dependent, the curve for log (BET area) versus temperature (see Fig. V.4) being roughly linear within the limits of experimental error. Also, as stated previously, typical BET values for fissile particles average perhaps 0.25 m^2/g when deposited from propane at 1400°C, and therefore the value of 0.34 m^2/g from propylene at 1400°C (Table V.2) is obviously considerably higher, equivalent to a deposition temperature difference of about 100°C (see

Fig. V.3--Curve showing typical volume displacement and pressure obtained during porosimetry on fuel particles

Fig. V.4--Specific surface area versus deposition temperature for LTI coatings deposited from propylene and propane

Table V.1
POROSITY CHARACTERISTICS OF F-25 PARTICLE BATCHES

Batch No.	Particle Type	LTI Coating Gas	Coating Temp ^a (°C)	Particle Outer PyC Density (g/cm ³)	Critical Pressure (psia)	Maximum Accessible Pore Diameter (μm)	Specific Surface Area (m ² /g)
4000-182	Fissile	Propane	1420	1.88	750	0.4	0.27
4000-183	Fissile	Propylene	1485	1.84	1300	0.18	0.88
4000-185	Fertile	Propane	1540	1.86	~750	~0.4	0.15
4000-184	Fertile	Propylene	1485	1.87	~1000 ^b	~0.26	0.41

^aTaken by Ircon pyrometer.

^bExtremely little porosity observed in this batch below 2500 psia.

Table V.2
SPECIFIC SURFACE AREAS OF FISSILE PARTICLE
BATCHES COATED USING PROPYLENE

Batch No.	Coating Temp (°C)	Specific Surf. Area (m ² /g)
4483-105	1100	0.095
4483-103	1200	0.25
4483-101	1300	0.23
4493-9	1350	0.31
4483-99	1400	0.34
4493-11	1450	0.51
4483-97	1500	0.60
4493-13	1550	1.74 ^a
4483-95	1600	1.80
4493-15	1650	1.20
4493-19	1700	1.90
4493-23	1800	2.79

^aProbably an invalid value because of an experimental oversight.

Fig. V.4). Based on Fig. V.4, the reported coating temperature of 1485°C for propylene-coated batch 4000-183 would be expected to give a BET value of about 0.14 m²/g, but the equivalent propane batch 4000-182 coated at 1420°C would only give about 0.28 m²/g. This is seen to be approximately the case in Table V.1. It is not possible to compare the results for the fertile particles, directly, but the same general trend and explanation is thought to be true.

In general, therefore, it is concluded that for the same deposition temperature, propylene-derived coatings will give greater surface area and finer porosity than propane coatings. This may be of importance with regard to irradiation stability of the coatings. The actual ratios of measured surface area divided by calculated surface area (assuming solid spherical particles) are 45 to 70 for propane coatings and about 200 for propylene coatings.

BAF Standardization

Work is in progress to produce permanent standard samples and construct calibration curves that will correlate the BAF determined on the existing Jarrel-Ash densitometer with that determined on a new Jarrel-Ash densitometer that will replace the present instrument. The Jarrel-Ash densitometers serve as a standard for the circular scanning densitometer used for routine BAF determination.

Permanent mounts have been fabricated by drilling a 1/8-in.-diameter hole in a 2 by 2 by 1/16-in. steel plate and mounting stacks of appropriate 0.010-in.-wide PyC strips from several disks over the hole. The strips are permanently held in place with cement at the ends. Two permanent mounts from each of 11 PyC batches have been fabricated. The batch number and nominal BAF are shown in Table V.3. A range of materials yielding BAF values from about 1.00 to 9.00 were selected.

Table V.3
PyC BATCHES SELECTED FOR PERMANENT
MOUNTING AS BAF STANDARD SAMPLES

Batch No.	BAF	Batch No.	BAF
3516-43	0.97	4344-7	1.18
4554-45	1.00	3506-151	2.97
4413-79	1.01	4273-15	4.28
3516-51	1.03	4273-19	7.66
4554-47	1.04	4273-17	9.19
4554-39	1.10		

During this report period X-ray exposure and densitometer scanning work with the old Jarrel-Ash densitometer were completed, and about 50% of the data have been reduced to a final BAF value. The procedure in this work has been to make five or six X-ray diffraction exposures for each of the two samples from each of the 11 batches. The BAF is then determined for each exposure. The standard deviation in BAF values for any one sample has been about 0.02, which is consistent with past experience. An example of the distribution of BAF data points is shown in Fig. V.5. There is one example (3516-51) of the BAF from one sample being consistently lower than the other sample from the same batch. Examination of the mount revealed that the sample yielding lower BAF values (sample A) contained a much shorter stack of PyC strips than sample B. The two mounts are shown in Fig. V.6. The X-ray exposure time for the two mounts was the same (about 1.5 hr), and probably as a result of the smaller sample volume, the X-ray film density for sample A was much less than for sample B. The result was a significant lowering of BAF in sample A. In order to correct this condition, a longer X-ray exposure time for sample A must be used to increase X-ray film density. As long as the sample width is 0.010 in. or less, stack height is not important from an absorption correction standpoint, but film density must be maintained at a standard value from specimen to specimen. This serves to illustrate one of the several variables which contribute to the variation and error in BAF determination. Unfortunately the accuracy of BAF values is heavily dependent on the skill and judgement of the person making the determination. However, the presence of permanent samples will make possible a check of operating procedures to ensure the reproducibility and accuracy of the BAF determinations.

Fig. V.5--Example of distribution of BAF data points obtained from standard BAF samples

(M-31091-1)

(25X)

(M-31092-1)

(25X)

Fig. V.6--Variation in stack height of 0.010 wide PyC strips in BAF mounts which produced significant variation in BAF determination when equal X-ray exposure times were used. The upper photo shows a single 40- μ m-thick strip of 3516-51; the lower shows four 40- μ m-thick strips of 3516-51

Coating Contamination Studies

It has been demonstrated (see previous quarterly report GA-9440) that (1) the release of fission gases from intact TRISO coated particles is almost entirely the result of fuel contamination on the surfaces of particles and (2) the release of metallic fission products is the result of diffusion of these elements from defective particles (particles with broken or missing SiC layers) and from fuel contamination on and within the outer coatings of the particles.

It has also been demonstrated (GA-9440) that levels of surface uranium contamination can be determined by a thin-film alpha-counting technique.* However, some difficulties have been encountered in trying to correlate surface uranium contamination and alpha counting data obtained by counting bulk particle samples.† Some of the difficulty can be attributed to thorium contamination of the particle surface. In a gross alpha count this would tend to give a high value of uranium contamination. The use of alpha spectrometry is being investigated, and a much improved correlation between uranium contamination and alpha-count rate is being observed. Work in this area is continuing.

Work is in progress to try to correlate the surface uranium contamination (determined by alpha-counting) of TRISO coated particles with the fraction of defective particles (particles with missing or broken SiC layers). The method previously employed for the determination of defective particles consisted of burning off the outer PyC layer and leaching the particles. The leach solutions were then alpha counted. Samples with a high percentage of uncoated fuel (defective SiC layers) showed a high alpha-count rate. This method is not very desirable, because of the destructive nature of the test and the time-consuming procedure.

THERMAL STABILITY OF COATED PARTICLES

The purpose of this work is to determine the mechanisms for chemical interaction of core and coating in TRISO coated fuel. The ultimate goal is to establish more accurately the operating limits of time and temperature for coated fuel particles. Effort has been concentrated on out-of-pile heating experiments, in which nominal temperature and magnitude of thermal gradient are the primary variables.

* In the thin-film alpha-counting technique, the particles are leached with acid (HClO_4 : HCl = 10:1) and the solution is evaporated to dryness on a glass planchet. The resulting thin-film residue is alpha counted. Appropriate standards are used for calibration purposes.

† A gross alpha count is made directly on a sample of fuel particles held on a standard counting planchet.

Thermal Gradient Experiments

Two experiments in progress during the previous two quarters, experiment 4283-72 and 4464-4, were continued during this report period. In this work, beds of TRISO coated particles were heated in a thermal gradient and radiographically examined at intervals to detect changes in distribution of Si, C, Th, and U.

The apparatus and specimens used in experiment 4283-72 were described in an earlier quarterly report (GA-9261). Heating of particles continued in helium at 5 psig in a thermal gradient of 1400°C/in. (5° to 25° per particle) at a nominal temperature of 1400° to 1560°C. Radiographic examination of particles heated at 1440° to 1520°C was conducted at 4000 and 5000 hr of exposure. Particles from two coating batches, 3923-111 and 4000-889, continued to show migration (amoeba effect) of the kernel toward the hot side in about 30% of the particles.

An attempt is being made to correlate migration rate with kernel composition, coating parameters, thermal gradient temperature, and time at temperature. The correlation is complicated by the variability of the effect from particle to particle within a given batch heated under nominally the same conditions. A summary of experiment observations is shown in Table V.4.

Heating of these specimens is being continued with the next examination scheduled at 7000 hr.

Experiment 4464-4 is similar to 4283-72, except that a more severe thermal gradient of 2300°C/in. (15° to 75°C per particle) was employed with a greater temperature range in the particle bed, 1250° to 1600°C. The specimen descriptions and heating conditions were reported in an earlier quarterly report (GA-9261). The experiment was terminated at 2000 hr of heating when the loose beds of particles were accidentally spilled and the identity confused. However, the identity of bonded radiograph specimens for each particle batch was retained so that radiographic, metallographic, and microprobe analysis of representative particles from each batch was possible. A summary of experimental observations is shown in Table V.4.

Four particle coating batches contained UC₂ kernels from the same lot that had a relatively high Fe content (237 ppm Fe) as shown in Table V.5. When these particles were heated at 1400° to 1530°C in a thermal gradient, migration of heavy metal to the cool side SiC-inner PyC interface was observed by radiograph after 500 hr. The accumulation of heavy metal on the cool side continued to increase until the experiment was terminated at 2000 hr. A radiograph specimen was mounted and sectioned metallographically and then examined by electron microprobe. High concentrations of Fe and U were found at the SiC-inner PyC interface, and high Fe occurred at the buffer-kernel interface on the cool side. Neither Fe nor U were found outside the kernel on the hot side. The typical distribution of U and Fe in a particle heated in a thermal gradient is shown in Fig. V.7. No evidence of Si diffusion was found and there was no detectable gradient in U or C across the UC₂ kernel.

Table V.4
SUMMARY OF THERMAL GRADIENT HEATING EXPERIMENT
KERNEL MIGRATION MEASUREMENTS

Batch No.	Coating Type	Th/U Ratio of Carbide Kernel	Exp. No.	Heating Time (hr)	Thermal Gradient (°C/in.)	Temp Range Across Radiograph Sample (°C)	Kernel Migration Observed in Radiograph ^a (μm)			Remarks
							Cool Side	Mid	Hot Side	
3325-117	TRISO-II HTI	1.6	4464-4	2000	2300	1400-1530	0	2	2	20 particles observed; 40% showed migration.
			4283-72	2000	1400	1440-1520	0	0	0	20 particles observed.
			4283-72	5000	1400	1440-1520	0	0	0	
3325-119	TRISO-II HTI	7.8	4464-4	2000	2300	1400-1530	0	4	8	5 particles observed; 40% showed migration.
			4283-72	2000	1400	1440-1520	0	0	0	6 particles observed.
			4283-72	5000	1400	1440-1520	0	0	0	6 particles observed.
4273-145	TRISO-II LTI	ThC ₂	4464-4	2000	2300	1400-1530	0	0	0	4 particles observed.
			4283-72	2000	1400	1440-1520	0	0	0	4 particles observed.
			4283-72	5000	1400	1440-1520	0	0	0	4 particles observed.
3923-111	TRISO-II HTI	2.75	4464-4	2000	2300	1400-1530	4	13	18	20 particles observed; 90% showed migration.
			4283-72	2000	1400	1440-1520	0	4	6	16 particles observed; 30% showed migration.
			4283-72	5000	1400	1440-1520	0	12	15	
4000-889	TRISO-II LTI	2.75	4464-4	2000	2300	1400-1530	4	4	13	15 particles observed; 50% showed migration.
			4283-72	2000	1400	1440-1520	0	2	2	15 particles observed; 30% showed migration.
			4283-72	5000	1400	1440-1520	4	5	9	
3592-35	TRISO-I LTI	2	4464-4	2000	2300	1400-1530	13	22	22	30 particles observed; 95% showed migration.
4413-135	TRISO-II HTI	UC ₂	4464-4	2000	2300	1400-1530	0	0	0	N ₂ carrier gas, 237 ppm Fe in kernel Fe-U migration to cool side particle, 16 particles observed.
4413-137	TRISO-II LTI	UC ₂	4464-4	2000	2300	1400-1530	4	9	9	N ₂ carrier gas, 237 ppm Fe in kernel, Fe-U migration to cool side of particle, 13 particles observed; 50% showed migration.
4413-151	TRISO-II HTI	UC ₂	4464-4	2000	2300	1400-1530	0	0	2	He carrier gas, 237 ppm Fe in kernel, Fe-U migration to cool side of particle, 19 particles observed; 5% showed migration.
4423-3	TRISO-II LTI	UC ₂	4464-4	2000	2300	1400-1530	4	5	13	He carrier gas, 237 ppm Fe in kernel, Fe-U migration to cool side of particle, 13 particles observed; 70% showed migration.

^aThe reported migration distance was obtained by measuring the thickness of the carbon precipitate that formed on the cool side of the kernel and was visible in the contact radiograph. Not all particles in a given temperature region showed migration. The reported migration distances are averages of the measured migration in those particles within a batch which exhibited migration.

(M-30066-133)

(200X)

(M-30427-3)

(200X)

Fig. V.7--Migration of Fe, U, and C in a TRISO II coated UC_2 particle (4423-3) after heating 2000 hr at about $1500^\circ C$ in a thermal gradient of $2300^\circ C/in$. Distribution of elements was determined by microprobe on similar particles. At left is a radiograph, prior to metallographic section; bright regions show presence of heavy elements. At right is a bright field photomicrograph of particle (mirror image of radiograph)

Table V.5

SPECTROCHEMICAL ANALYSIS OF UC₂ KERNELS (XDS 3591)
COATED IN BATCHES 4413-135D, 4413-137D, 4413-151D, 4423-3D

Element	Concentration (ppm)	Element	Concentration (ppm)
Si	<15	Cu	>40
Cr	<30	Pb	<40
Al	5	Ni	<20
Mg	3.9	Ag	<0.3
Fe	237.7		

A spectral scan for elements with atomic numbers greater than Na revealed no impurities other than Fe.

An attempt to correlate kernel and coating parameters with observed thermal effects is in progress. However, some general observations about the experimental results of the two experiments (4283-72 and 4464-4) are given below:

1. The use of N₂ or He as a carrier gas had little influence on the thermal effects in UC₂ coated particles.
2. An Fe content of 237 ppm or higher in the kernel probably would not be acceptable for HTGR fuel because of rapid migration of Fe and U to the SiC coating on the cool side with probable detrimental effects. Kernels with Fe content of 100 ppm or less have not shown this effect in thermal gradients and should be acceptable for HTGR fuel.
3. No migration of U or Th through SiC barrier coatings has been observed, although migration of Th and U into the buffer and inner PyC does take place.
4. An increase in thermal gradient causes the migration rate at a given temperature to increase. The thermal gradients employed here were approximately the maximum expected in HTGR operation and therefore represent extreme conditions.
5. In particles with kernels from the same batch, those with LTI coatings show a greater tendency for kernel migration (carbon diffusion) than those with HTI coatings.

Experiment 4464-25 is a long-term intermediate-temperature (1000° to 1300°C) experiment in which samples of 14 different TRISO coating batches will be heated in a thermal gradient. The selected batches and purpose of each is shown in Table V.6.

Each sample was divided into three portions and poured into shallow 1/8-in.-diameter flat-bottom holes drilled in rings of B-204 insulating carbon. The monolayers of particles were cemented in place with a phenolic resin and carbonized at 1400°C for 30 min.

Table V.6
TRISO-II SPECIMENS SELECTED FOR EXPERIMENT 4464-25

Purpose of Series	Batch No.	Coating Type	Carbide Kernel Th/U Ratio	Remarks
Compare propylene versus propane coatings, effect of heat treatment	4000-183	LTI-propylene	2.75	Heated 30 min, 2000°C after coating.
	4000-183-2000	LTI-propylene	2.75	
	4000-180	LTI-propane	2.75	
	4000-184	LTI-propane	ThC ₂	Heated 30 min, 2000°C after coating.
	4000-184-2000	LTI-propylene	ThC ₂	
	4000-176	LTI-propane	ThC ₂	
Compare Si doped, Si overcoat, and SiC overcoat type coatings	4042-95	HTI-methane	ThC ₂	Si-doped outer PyC.
	4452-149	LTI-propane	ThC ₂	Si overcoat outer PyC.
	4632-43D	LTI-propylene	4	SiC overcoat outer PyC.
Compare N ₂ versus He as carrier gas, effects of Fe and Ru impurities in kernel	4413-137	LTI-propane	UC ₂	N ₂ carrier gas; Fe in kernel (237 ppm Fe).
	4423-3	LTI-propane	UC ₂	Fe in kernel (237 ppm Fe).
	4423-54	LTI-propane	4	Fe in kernel (>1000 ppm Fe).
	4423-51	LTI-propane	4	Ru in kernel (2750 ppm Ru).
	4423-41	LTI-propane	4	Control sample for series.
Compare coating batch showing maximum amoeba effect with current particles	3592-35	LTI-propane	2	TRISO-I, showed maximum amoeba migration rate in P13J capsule and subsequent heating experiments.
Evaluate particles with propylene-derived buffer and PyC	T815(A)SL	LTI-propylene	ThC ₂	Propylene buffer and inner PyC.
	ET814(A)S	LTI-propylene	2.75	Propylene buffer and inner PyC.

Three duplicate carbon rings containing 18 different batches were stacked and loaded into a B-204 carbon crucible that contained sight holes for temperature measurement of inner and outer walls. A disassembled and assembled view of a typical set of specimens is shown in Fig. V.8. Four of the loaded crucibles were stacked and heated by a graphite electrode that passed through the center hole of the crucible. This experiment will be examined at 500, 1000, 2000, and 5000 hr and at 5000-hr intervals thereafter. Examination will consist of taking contact radiographs and measuring the extent of heavy-metal migration. Radiographs are taken by placing the carbon ring directly on the photographic emulsion and passing X-rays through the particles, bottom of the 1/8-in. hole, and into the emulsion.

About 100 hr of heating time has been accumulated in this experiment.

ANALYTICAL STUDIES OF COATING DESIGN

Studies of the integrity of TRISO-coated fuel are being conducted using mathematical models. The mathematical model used in these TRISO studies is that of Kaae (Refs. 1, 2). The Kaae model is an 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 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.* (Ref. 3). The remainder of the input parameters are the same as those reported in an earlier quarterly report (GA-8879).

Stress design limits in each layer of the coating were established by calculating stresses in samples irradiated in P-capsules and determining correlations between calculated stresses and the observed performance of the samples.

The models are used to conduct parametric studies. Coating stresses are calculated for various coating geometries, PyC densities, and irradiation temperatures. The acceptable ranges of design parameters were established by comparing the stresses with the limiting values indicated by P-capsule results.

Parametric Studies

In previous quarterly reports, the results of a parametric study on TRISO-II coated particles were reported. The narrow range of acceptable density of the inner isotropic layer was the limiting feature of the fissile particle design. In the fertile particle design, silicon carbide stress at the end of life, above 1050°C, was a potential problem. Various design modifications had been investigated to relieve these difficulties. The previous modifications generally led to an increase in total coating thickness and hence to a reduction in fuel volume loading per particle. A parametric study was undertaken to determine whether or not the total coating thicknesses could be reduced to increase fuel volume loading per particle and yet maintain acceptable calculated coating stress levels.

(ME-74693)

(ME-74690)

Fig. V.8--Typical specimens and crucibles used in experiment 4464-25. Particles were cemented in place with carbonized phenolic resin

The analysis has shown that, from a calculated stress standpoint, the coating thicknesses of the fissile-B and fertile-B particles can be reduced from 130 to 95 μm and from 160 to 115 μm , respectively. The coating on fissile-B particles would thus consist of a 40- μm buffer, 10- μm inner PyC, 15- μm SiC, and 30- μm outer PyC. The acceptable inner PyC density range is 1.82 to 1.91 g/cm^3 , and the acceptable outer PyC density range is 1.79 to 1.91 g/cm^3 . The coating on fertile-B particles would consist of a 60- μm buffer, 10- μm inner PyC, 15- μm SiC, and 30- μm outer PyC. The acceptable density ranges are 1.86 to 1.90 g/cm^3 for the inner PyC and 1.80 to 1.94 g/cm^3 for the outer PyC.

Stress Calculations for P-capsule Samples

Capsule P23. Stress calculations were performed on the BISO and TRISO-II coated particle samples irradiated in the P23 capsule in an attempt to correlate the observed performance with calculated stresses. Because of the scarcity of input data for LTI coatings, all calculations were performed using HTI data. Since inner isotropic layer densities had not been measured in all cases, calculations were performed using two estimated values for the inner-layer density of the TRISO-II particles indicated a value between 1.75 and 1.90 g/cm^3 .

The calculations indicate that all BISO coated particles in capsule P23 should be expected to survive at the irradiating temperature shown in Table V.7, out to a dose of 8×10^{21} n/cm^2 . Two of the BISO design (with 1.79 g/cm^3 PyC coating), however, exhibited a calculated net growth of +0.4% at that dose.

The dose that the TRISO-II coated particles received is in the range where initial PyC densification occurs. This densification leads to a calculated stress peak in the TRISO-II coating. Consequently, a number of designs show calculated PyC stresses higher than the 20 kpsi stress limit at the dose received, and yet survived as determined by postirradiation examination. The P23 experiment offers more evidence that the maximum allowable early in life stress buildup could be more than the 20 kpsi limit imposed later in life. Calculated stresses due to early densification are generally reduced with further irradiation and then may or may not increase again, depending upon irradiation conditions and coating configurations. At higher doses than those actually received, the restrictions on the use of the TRISO-II coated particles in P23 are posed typically by calculated stresses in the SiC layer, with two exceptions. From Table V.7 it can be seen that under various conditions both SiC and outer PyC layer difficulties could be expected with particles 4000-889 and 4000-711.

FISSION-PRODUCT RELEASE

The effect of temperature on fission-gas release values of BISO and TRISO coated fissile fuel particles is being studied further. As reported earlier (see quarterly report GA-9440) fission-gas release values were obtained for BISO and TRISO coated fuel particles at room temperature and over the temperature range 1000° to 1700°C. These data indicate an increase in gas release occurring near 1000°C for both BISO and TRISO coatings. Similar release behavior is expected for the two types of coatings, because the

Table V.7
CALCULATED VALUES FOR COATED PARTICLES TESTED IN CAPSULE P23
(Two-cycle Irradiation)

Lot No.	Particle Coating								Irradiation Conditions			Calculated Values												Postirradiation Examination	
	Buffer		Inner Isotropic			SiC	Outer Isotropic					Stress at Fast Dose (10 ³ psi)			Stress at 8 x 10 ²¹ n/cm ² (10 ³ psi)			Stress Limit ^b Dose (10 ²¹ n/cm ²)		Max. Stress Early in Life					
	Thck. (μm)	Density (g/cm ³)	Thck. (μm)	Density (g/cm ³)	Density ^a (g/cm ³)	Thck. (μm)	Thck. (μm)	Density (g/cm ³)	Density ^a (g/cm ³)	Temp (°C)	Calc ^a	Dose (10 ²¹ n/cm ²)	Inner	SiC	Outer	Inner	SiC	Outer	SiC	Outer	Inner	Outer	Micro. Exam		
																						(% Fail)	(% Fail)		
4263-91E ^c	48	1.23	22	1.87	1.87	19	28	1.55	1.55	1245 950	1200 1050 900	2.1 1.8 1.8	11.5 20.1 23.8	-19.8 -48.5 -65.4	9.5 20.1 27.9	11.7 3.5 16.0	8.6 19.6 -5.4	4.1 3.1 9.7	7.2 5.33 >8	>8 >8 >8	23.5 28.7 25.8	25 25 30	0 0 0	0 0 0	
4263-81E ^c	48	1.23	22	1.87	1.87	19	30	1.76	1.76-1.81	1245 950 ^d	1200 1050 900	2.1 1.8 1.8	11.3 19.8 23.4	-29.7 -40.1 -50.4	14.2 13.5 17.4	11.8 3.4 15.9	8.0 4.3 -20.4	7.3 17.3 13.3	7.55 >8 >8	>8 >8 >8	24. 29.0 26.1	<20 <20 <20	0 0 0	0 0 0	
4263-83E ^c	48	1.23	22	1.87	1.87	19	31	1.86	1.87	1245 950 ^d	1200 1050 900	2.1 1.8 1.8	11.4 20.0 23.5	-19.6 -38.3 -42.6	8.6 12.0 12.8	11.7 3.4 15.7	-14.1 1.7 -30.0	19.8 17.7 19.1	>8 >8 >8	>8 >8 >8	24.0 29.0 26.2	<20 <20 <20	0 0 0	0 0 0	
4263-137E ^c	48	1.25	20	1.76	1.76-1.81	21	28	1.88	1.87	1230 950 ^d	1200 1050 900	2.1 1.8 1.8	20.8 20.7 30.7	-24.6 -31.7 -41.2	8.7 12.1 13.0	-6.1 7.4 9.5	5.3 -3.2 -19.2	19.5 17.0 18.9	7.94 >8 >8	>8 >8 >8	30. 34.3 33.	<20 <20 <20	0 0 0	0 0 0	
4263-77E	48	1.23	22	1.87	1.87	19	31	1.53	1.55	1240 905	1200 900	2.3 2.0	9.7 22.2	-16.3 -61.0	8.0 23.4	11.7 14.0	8.0 -4.0	4.1 5.5	7.2 >8	>8 >8	23.9 25.7	24.5 34.5	0 0	0 0	
4263-61E	45	1.25	16	1.85	1.87	21	32	1.64	1.55	1240 910	1200 900	2.3 2.0	8.5 19.9	-10.9 -48.5	7.9 23.0	10.5 14.8	10.6 -0.07	2.2 4.5	5.99 (5.6)	>8 >8	21.8 23.6	24.3 34.1	0.1 0	--- 0	
4263-59E	45	1.25	16	1.85	1.87	21	30	1.73	1.79	1240 920	1200 900	2.3 2.0	8.4 19.4	-20.8 -36.7	14.7 16.2	10.6 14.7	10.7 -13.7	7.5 13.4	6.9 >8	>8 >8	20. 24.	<20 <20	0 0	0 1 ^e	
4263-75E	48	1.23	22	1.87	1.87	19	30	1.83	1.79	1260 900 ^d	1200 900	2.1 1.8	11.3 21.1	-29.7 -37.9	14.2 17.3	11.7 14.7	7.9 -13.3	7.5 13.3	7.6 >8	>8 >8	23.9 23.9	<20 <20	0 0	0 0	
4000-889	44	1.08	11	(f)	1.79 1.87 1.79 1.87	16	48	1.92	1.93	1005	1050 1050 900 900	1.4 1.4 1.4 1.4	17.2 20.0 28.2 21.5	-32.6 -34.1 -41.2 -36.9	8.8 8.7 9.2 9.2	7.2 3.8 8.7 14.3	-34.2 -29.7 -45.8 -48.2	24.6 24.6 24.6 24.6	>8 >8 >8 >8	4.77 4.77 5.8 5.8	30.0 24.5 28.4 22.1	<20 <20 <20 <20	0	3	
4000-711 ^g	41	1.05	18	(f)	1.79 1.87 1.79 1.87	20	45	1.90	1.93	1005	1050 1050 900 900	1.4 1.4 1.4 1.4	15.2 17.9 32.0 24.5	-14.1 -15.7 -44.3 -38.2	10.5 10.4 9.5 9.5	-5.5 -12.1 9.3 15.0	73. 101. -36.4 -39.4	20.2 20.3 23.9 23.9	2.6 2.53 >8 >8	7.4 7.2 6.1 6.1	29.2 23.7 32.3 24.9	<20 <20 <20 <20	0	(h)	
4263-25E	61	1.30	--	--	--	--	61	1.54	1.55	1245 905	1200 900	2.3 2.0	-- --	-- --	5.3 4.2	-- --	6.5 7.9	-- --	>8 >8	-- --	-- --	0 0	0 0		
4263-27E	61	1.30	--	--	--	--	53	1.68	1.55 1.79 1.55 1.79	1240 1240 910 910	1200 1200 900 900	2.3 2.3 2.0 2.0	-- -- -- --	-- -- -- --	6.2 8.3 4.6 8.0	-- -- -- --	6.2 14.1 8.1 12.6	-- (1) -- >8	-- -- -- --	-- -- -- --	0 0 0 0	0 0 0 0			
4263-37E	61	1.30	--	--	--	--	60	1.73	1.79	1240 950	1200 900	2.3 2.0	-- --	-- --	8.0 3.4	-- --	14.4 12.8	-- >8	(1) --	-- --	-- --	0 0	0 0.3		

NOTE: All particles assumed spheroidal. Kernel diameter was 200 μm, except as noted. HTI pyrocarbon data used throughout.

^aValues used in calculation.

^b20 kpsi PyC; 5 kpsi SiC.

^cParticles with HTI outer PyC coatings; all others had LTI coatings but these were treated as HTI.

^dEstimated.

^eFailed particles had fabrication defects.

^fNot determined.

^g400-μm kernel.

^hVery few particles were observed metallographically.

ⁱNet growth of about 0.4% at 8 x 10²¹ n/cm².

source of fission-gas release in both cases is uranium contamination in the outer pyrolytic carbon coatings. Additional data in the temperature range 400° to 1000°C are presently being obtained using thermocouples to measure the lower temperatures. Final results are expected shortly.

PHASE BEHAVIOR OF THE Th-Si-C AND U-Si-C SYSTEMS

Annealing experiments with samples from the Th-Si-C and U-Si-C systems, using ThSi_2 , USi_2 , and carbon as starting materials have been concluded. As reported earlier (GA-9440), annealing of samples of ThSi_2 with carbon and USi_2 with carbon at 1800°C (above the silicide melting point) for 1 hr produced the actinide carbide and silicon carbide. Annealing experiments conducted at 1400°C (below the silicide melting point) for 150 hr, using similar samples of the silicide and carbon, showed three phases to be present, the actinide carbide, silicon carbide, and an unidentified third phase. The recent annealing experiments were undertaken to identify the unknown phase.

The samples of ThSi_2 plus carbon and USi_2 plus carbon used in the 150-hr 1400°C anneals were reannealed at 1400°C for an additional 400 hr. X-ray analysis of the samples after reannealing showed a large decrease in the X-ray diffraction pattern for the unknown phase in both the uranium and thorium samples and a corresponding increase in line intensity for the actinide carbide and silicon carbide phases. It has been concluded from these data that the unknown phase is a silicide of different composition and that the existence of all three phases in the 1400°C anneal represents a nonequilibrium situation indicating insufficient annealing time. It has also been concluded that SiC and ThC_2 (or UC_2) are the stable phases in the Th (or U)-Si-C system. Annealing experiments have been terminated.

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