GULF GENERAL ATOMIC

GA-10560

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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
MARCH 31, 1971

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

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ABSTRACT

This quarterly report is a continuation of the series describing the research and development program for the Public Service Company of Colorado 330-MW(e) High-temperature Gas-cooled Reactor. The work reported here includes studies of component development, such as auxiliary equipment and special tools, and nuclear analysis of the fuel cycle, fuel zoning, and power shaping. The fuel development portion of the report includes studies of fuel element design, core materials development and testing, and demonstration testing of the performance of test fuel elements by means of irradiation at expected operating temperatures. Studies covering the components of the steam generator under operating conditions are included. In addition, there is a continuing study of process variables for the fabrication of coated fuel particles. Experimental procedures and results are discussed and, where appropriate, the data are presented in tables, graphs, and photographs. More detailed descriptions of experimental work are presented in topical reports, and these are listed at the end of the report for those concerned with the field.

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 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

AUXILIARY EQUIPMENT AND SPECIAL TOOLS

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

Reserve Shutdown Vacuum Tool

The reserve shutdown vacuum tool environmental test was completed during this period. Three types of test were conducted: an insertion and withdrawal test, a test in ambient-temperature helium, and a test in 165°F helium. The first test consisted of inserting and withdrawing the tool in the core when the reserve shutdown channel was empty. This test was performed (1) with the core aligned to the refueling penetration and (2) with the core offset from the penetration. The helium compressor was not operated during this test.

The second test was conducted in a room-temperature helium atmosphere at a reduced pressure of 9.2 psia. In this test simulated neutron absorber material (graphite balls) was placed in the reserve shutdown channel. The helium compressor was powered, and the tool was inserted in the core to remove the absorber material.

The third test was conducted in 165°F helium at a reduced pressure of 12.5 psia. Simulated neutron absorber material was placed in the reserve shutdown channel. The helium compressor was powered, and the tool was inserted in the core to remove the absorber material.

Insertion and Withdrawal Test. The tool was inserted and withdrawn through the core with the handling equipment shown in Fig. 1.1. At the full-down position the tip of the nozzle was 18 ft 11 in. below the top of the core.

Fig. 1.1. Handling equipment setup for vacuum tool insertion and withdrawal test

The test results obtained for four core positions are given in Table 1.1. Only one problem occurred in this test. In the 1-5/8-in. core offset configuration, the tool required alignment to the guide sleeve as it was withdrawn into the sleeve. Before making this alignment, the housing keyway centerline was offset from the guide sleeve keys centerline by approximately 26°.

Test in Ambient Temperature Helium at 9.2 psia. The compressor motor was energized, and after approximately 40 sec the compressor reached operating speed. The ammeter was pegged full scale (75 amp) until the compressor operating speed was achieved. The absorber material (88 lb 10 oz) had extended to within 3 in. from the top of the core before tool insertion began.

No problems occurred as the tool was inserted into the core. The tool's traverse into the core was stopped for a short time on about three occasions to confirm test readings. The timer control in the motor-generator unit automatically secured power to the compressor motor after 4 min 49 sec running time on the compressor. This was the maximum running time permitted by the timer control. After a 1-hr rest period (cooldown), the test was resumed to achieve full traverse (18 ft 9 in.). All of the absorber material was removed from the channel with the vacuum tool (the test data are given in Table 1.2). An appreciable amount of dust was found on the tool when it was removed from the autoclave. A very small quantity (considerably less than 1%) of the graphite balls was damaged (fractured).

Test in 165°F Helium at 12.5 psia. The compressor motor was energized and reached operating speed in approximately 40 sec. Insertion of the tool was begun immediately and was continued without stopping over the full traverse (18 ft 9 in.), the average velocity of the tool during insertion being 8.8 ft/min. No problems occurred during the test, and all of the absorber material (88 lb and 10 oz) was removed from the channel with the vacuum tool (the test data are given in Table 1.3). Additional dust was found on the tool when it was removed from the autoclave. A very small quantity (considerably less than 1%) of the graphite balls was damaged (fractured).

TABLE 1.1
VACUUM TOOL INSERTION AND WITHDRAWAL TEST

Graphite Column Offset (in.)	Nozzle Position (a)	Scale Readings During Insertion		Scale Readings During Withdrawal	
		Tool Stationary (1b)	Tool Moving (1b)	Tool Stationary (1b)	Tool Moving (1b)
0	2 in. to 6 ft (I)	1203	1168	---	---
0	8 ft to 18 ft-11 in. (I)	1303	1178-1228	---	---
0	0 ft to 18 ft-9 in. (W)	---	---	1253-1303	1403
3/4	2 in. to 18 ft-11 in. (I)	1278-1303	1078-1153	---	---
3/4	0 ft to 18 ft-9 in. (W)	---	---	1303	1378-1428
1-1/2	2 in. to 18 ft-11 in. (I)	1303	1153-1203	---	---
1-1/2	0 ft to 18 ft-9 in. (W)	---	---	1303	1378-1403
1-5/8	2 in. to 18 ft-11 in. (I)	1278-1303	1178-1203	---	---
1-5/8	0 ft to 18 ft-9 in. (W)	---	---	1303	1403-1428

(a) (I) = Inserted, (W) = Withdrawn.

TABLE 1.2
TEST IN AMBIENT TEMPERATURE HELIUM AT 9.2 PSIA

Time (min-sec)	Nozzle Position (ft)	Scale Reading During Insertion		Compressor Motor		Remarks
		Tool Stationary (lb)	Tool Moving (lb)	amp	volt	
0-0	0	1325	1300-1400	---	---	Insertion begun
---	1	1325	1300-1400	28.0	318	
0-45	2	1325	1300-1400	31.3	323	
---	3	1325	1300-1400	31.3	323	
---	4	1325	1300-1400	31.3	323	
1-0	5	1325	1300-1400	31.3	327	
1-15	6	1325	1300-1400	28.0	325	Insertion stopped for 10 sec
---	7	1325	1300-1400	31.3	327	
---	8	1325	1300-1400	31.3	327	
2-0 ^(a)	9	1325	1300-1400	31.3	330	
---	10	1325	1300-1400	31.3	330	
2-30	11	1325	1300-1400	31.3	330	
---	12	1325	1300-1400	31.3	327	
---	13	1325	1300-1400	31.3	330	
---	14	1325	1300-1400	31.3	330	
---	15	1325	1300-1400	31.3	330	
3-0	16	1325	1300-1400	31.3	330	
3-15	17	1325	1300-1400	31.3	330	Timer secured power to com- pressor after 4 min 49 sec
---	17	1325	1300-1400	31.3	332	Test restarted after 1-hr cool- down
---	18' 11"	1325	1300-1400	31.3	332	

NOTE: (1) The tool insertion was intermittent during test. (2) All of the simulated neutron absorber material (88 lb 10 oz) was removed from the channel with the vacuum tool.

(a) 8-1/2 ft.

TABLE 1.3
TEST IN 165°F HELIUM AT 12.5 PSIA

Time (min-sec)	Position (ft)	Scale Reading During Insertion		Compressor Motor		Remarks
		Tool Stationary (lb)	Tool Moving (lb)	amp	volt	
0-0	0	1325	---	37.5	350	Commenced inserting tool and continued without stopping
---	1	---	---	---	---	
---	2	---	---	37.5	350	
---	3	---	---	---	---	
0-30	4	---	1250	37.5	350	
---	5	---	---	---	---	
0-40	6	---	1250	37.5	358	
---	7	---	---	---	---	
0-55	8	---	1275	42.1	358	
---	9	---	---	---	---	
1-8	10	---	1275	42.1	358	
---	11	---	---	---	---	
1-22	12	---	1275	40.0	358	
---	13	---	---	---	---	
1-35	14	---	1300	40.0	358	
---	15	---	---	---	---	
---	16	---	---	40.0	358	
---	17	---	---	---	---	
2-5	18	---	---	38.0	358	
2-8	18' 11"	---	---	38.0	352	

NOTE: (1) The tool insertion was continuous during test. (2) All of the simulated neutron absorber material (88 lb 10 oz) was removed from the channel with the vacuum tool.

Reactor Viewing Device

The analysis of the fuel handling machine viewing device cooling problem that was initiated during the previous quarter (see earlier quarterly report GA-10444) was completed during this period.

Analytical Approach. Thermal analysis of the reactor viewing device coolant supply line was performed in order (1) to develop a computer model using test results and (2) to evaluate possible system modifications to reduce the camera coolant temperature.

The thermal analysis was performed using TAC2D computer codes having cylindrical geometry. The models were constructed in order to take into account the three sections of flexible steel hose, the three sections of rigid tubing, the helium coolant, heat-transfer by convection, and heat-transfer by conduction between the fuel transfer mechanism and the coolant line at points of rigid mechanical mounting of the coolant line.

Two basic models were used: Model 1, which was relatively detailed (see Fig. 1.2), was used to ascertain the effects of thermal conduction but was incapable of properly handling radiant-heat transfer. Model 2, which was relatively simple (see Fig. 1.3), was capable of handling heat transfer by convection and radiation but not conduction through the mechanical attachment points on the lines. The results reported are based on both of these models; three basic cases were studied.

Case 1 was based on testing in a test tower with the fuel transfer mechanism at a vertical position of $Z = 600$ in. The environment was stagnant helium at 250°F .

Case 2 was modeled to simulate the reactor viewing device in position to survey the upper plenum of the reactor. The fuel transfer mechanism was positioned at $Z = 480$ in. The environment in the upper plenum was inlet helium at 165°F . The flow rate was 43,000 lb/hr at 12.3 psia.

Fig. 1.2. Model 1 for thermal analysis

Fig. 1.3. Model 2 for thermal analysis

Case 3, which was the most rigorous, was modeled to simulate the reactor viewing device at the lowest refueling position ($Z = 745$ in.). The helium environment varied between 165°F at the upper plenum to 185°F at the outlet of the region. The shutdown coolant flow rate was 6950 lb/hr through the removed region area. The fuel transfer mechanism with coolant line inside was also subjected to gamma heating and radiant heating from the adjacent core elements.

All three cases were analyzed for steady-state operation during extended periods of special operations of the fuel handling machine in the core region. During refueling the machine is raised and lowered between the hotter core area and the cooler cask area. Since this analysis is for periods of special operation, the fuel storage rack in the fuel handling machine was assumed to be empty and therefore was not a source of heat for the coolant line.

The coolant line in the fuel transfer mechanism is rigidly attached in several places. These paths for thermal conduction were modeled by using circumferential fins around the cooling line in five locations, as shown in Fig. 1.2. The fins were given a high heat-transfer coefficient at their vertical edge. The external coolant heated the fin and coolant lines in local areas. The conductance of each fin was determined in part by the thickness of the fin. The thickness of each fin was modeled based on the predicted heat-transfer characteristics of the actual conduction paths.

The external boundary temperatures were set in the model by assuming primary coolant inlet gas temperatures. The gradients in these boundaries were developed by setting relatively low flow rates in the boundary coolants and allowing the coolant stream to cool off as it traversed the coolant line.

In model 1 the heat-transfer area was modeled by utilizing different outside diameters for the flex hose and the hard line sections. In model 2 the modeled line is of a constant 0.5 -in. outside diameter. Heat transfer to the 0.25 outside diameter tubing portions of this model was compensated for by multiplying the external heat-transfer coefficient by $0.25/0.50$ to account for the area difference.

The results of the analysis are summarized in Table 1.4. These results were determined in the following manner: (1) The TAC2D code (model 2) was run to determine the expected coolant temperature due to radiant and convective heat transfer. (2) This temperature was increased to account for conduction to the coolant line from mechanical connections with the fuel transfer mechanism. The addition due to conduction is about $0.07^{\circ}\text{F}/^{\circ}\text{F}$ based on the average temperature difference between the coolant line in the fuel transfer mechanism and the external environment. This conduction factor was based on case 1 (tower test) results using model 1. (3) The heat input to the coolant was determined from the product of the total temperature rise determined in steps 1 and 2, the mass flow rate (38 lb/hr), and the specific heat of helium. (4) The expected vidicon operating temperature was determined from the nomogram shown in Fig. 1.4.

These results indicated that satisfactory steady-state operation of the existing modified viewing device can be expected with the television camera in the upper plenum if the temperature at the inlet to the fuel handling machine is 60° to 90°F . The corresponding expected steady-state operating temperature of the vidicon would be 138°F for an inlet temperature of 60°F .

The results presented in Table 1.4 also indicate that satisfactory steady-state operation of the existing modified viewing device positioned deep in the core is unlikely even for a coolant inlet temperature as low as 0°F . Run No. 5 indicates a satisfactory reactor viewing device temperature of 166°F when the coolant enters the fuel handling machine at -100°F .

Since an inlet temperature of -100°F may be undesirable from coolant line material compatibility or economic standpoints, a consideration of higher temperatures is of interest.

Two basic courses of action appear possible: (1) Helium coolant could be introduced to the viewing device system at 30° to 60°F . This would require moderate precooling of the supply helium, since the existing system supplies helium at approximately refueling room temperature (65° to 120°F).

TABLE 1.4
RESULTS OF REACTOR VIEWING DEVICE INLET COOLANT TEMPERATURE CALCULATIONS

Run No.	Case No.	Flow Rate (lb/hr)	FHM Position (in.)	Environ. Temp (°F) Max. Cool./Max. Rad.	FHM Coolant Temperatures (°F)			Heat Input To Coolant (BTU/hr)	Expected Vidicon Operating Temp (°F)
					Inlet	Outlet, Conduction Only	Outlet, Conduction + Convection		
1	1	38	600	250/250	60	121	130	3,298	169
2	2	38	480	165/165	60	98	103	2,026	138
3	3	38	745	185/600	60	174	178	5,560	209
4	3	38	745	185/600	0	152	160	7,539	192
5	3	38	745	185/600	-100	115	132	10,931	166

Fig. 1.4. Steady-state vidicon operating temperature

This system would provide steady-state operation above the core and useful transient operation of about 15 min for operation at the bottom of the core. This approach would require cooloff periods for the camera system. (2) More complex cooling could be added to lower the inlet temperature to around -100°F . This should provide steady-state operation of the reactor viewing device under all normal conditions. A less severe cooling requirement of about -60°F should provide steady-state operation if the coolant hard lines on the lower portion of the fuel transfer mechanism were insulated.

TASK II

NUCLEAR ANALYSIS

FUEL CYCLE ANALYSIS

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

Status of Nuclear Design

Experience gained during the production of the fissile and fertile particles for the initial core loading has indicated that the achievable metal density in the fuel rods may be somewhat lower than anticipated. The initial core had been deliberately designed with a particularly heavy uranium and thorium loading to obtain a large negative temperature coefficient and at the same time very low power peaking factors during the transition to the equilibrium core. Preliminary analysis indicated that a reduced metal loading of about 10% would not adversely affect core performance and would facilitate meeting fuel loading requirements. This preliminary analysis included calculations for the excess reactivity during the first year, the radial and axial power distribution, the control rod worth and shutdown margin, and the comparative peaking factors during the transition from the initial to the equilibrium cores. The lighter initial core loading was adopted as Reference Design 12, and all analytical work on the heavier loaded Reference Design 11 was stopped. A revision to the loading specifications was made so that fuel loading operations for the initial core might continue.

All of the physics startup test procedures for the fuel loading, zero-power tests, and the rise-to-power tests have been written and are being

reviewed by Gulf General Atomic and Public Service Company of Colorado.

Review of Reference Data and Cross Sections

Before proceeding with the analysis of the core with the reduced thorium and uranium loading, a brief review was made of the expected core atom densities and the effective group cross sections for the different core materials.

Although the exact amount of uranium and thorium that will be present in the core will be known within 1% because the total loadings for each of the compositions in the initial core are specified, this is not true for either the carbon or silicon. About 17% of the total carbon in the core is contained in the fuel rods as either matrix material or within the fissile and fertile particles. The total carbon load from the unfueled fuel elements is accurately known, because it is based on the carbon content of machined elements, as reported in detail in an earlier quarterly report (GA-10202). The total amount of carbon in the fuel rods, however, is somewhat uncertain considering the allowable tolerance ranges on the various coating layers. Similarly, the amount of silicon in the core may deviate from the assumed load because of tolerances on the average SiC layer thickness.

Since the production of fuel particles and rods is presently under way, some chemical analysis has become available for the material constituents in the fuel rods containing fuel composition No. 2. Although that analysis was based on only a few samples, some tentative conclusions can be drawn from it. The mass and density of the material within the fuel rod are summarized in Table 2.1 and are compared with that used in the analysis as obtained from the nominal particle and rod description. From the results of that comparison, it can be seen that

1. The uranium and thorium densities are in good agreement with the required loading. The allowable variation for the uranium and thorium between fuel rods is $\pm 7\%$, and these values are well within this amount.

TABLE 2.1
MATERIAL CONSTITUENTS OBTAINED FROM CHEMICAL ANALYSIS
OF A COMPOSITION NO. 2 FUEL ROD^(a)

Material	Total Mass in Fuel Rod (g)	Density from Chemical Analysis (g/cm ³)	Density from Nominal Particle Description (g/cm ³)
Carbon (matrix 0.63 packing fraction)	1.90	0.85 ^(b)	0.55
Total carbon (matrix and particles)	7.15	1.188	1.122
Uranium	0.125	0.0208	0.0218
Thorium	4.09	0.680	0.686
Silicon	1.21	0.201	0.178

(a) Fuel rods are 1.94 in. long and 0.49 in. in outside diameter.

(b) Exclusive of particles.

2. The measured matrix density is somewhat higher than the assumed nominal value. This causes an increase in the carbon density of about 5% within the fuel rod and of about 1% within the total core.
3. The measured silicon density is about 13% higher than the assumed nominal value.

Since the negative reactivity associated with the additional silicon would almost compensate for the positive reactivity resulting from the increased carbon content, no changes in assumed core atom densities in the calculational models were made pending further chemical analysis.

The effective broad group cross sections as described in detail for the Reference Design 11 core analysis (see earlier quarterly report GA-10313) should still be adequate for the analysis with the reduced core loading. The effective cross-section sets were obtained as a function of the thorium density within the fuel rods as well as the carbon-to-uranium ratio. Consequently, cross-section sets corresponding to specific fuel compositions with different thorium or uranium loading can easily be obtained by interpolating on this cross-section data. In fact, the fuel rod carbon density for that cross-section analysis was within 1% of the carbon density as measured from the chemical analysis (see Table 2.1).

Selection of Initial Core Loading

In order to minimize the power peaking during the transition to the equilibrium core, the initial core must contain a uranium loading heavier than the average equilibrium core. Power peaking can be measured by the age peaking factor, defined as the ratio of the fission density for each core reload segment to the average fission density in the total core. Consequently, no loading reduction resulting in a C/Th ratio greater than 200 was considered desirable for the initial core. An analysis was made to determine the feasibility of either a 5% or 10% metal loading reduction, which would give a C/Th ratio for the initial core of 186 or 197, respectively.

To maintain a satisfactory power distribution in the initial core, the loading reduction was nearly uniform for all of the 13 initial core compositions, and neither the axial nor radial fuel zoning was modified. In addition, the uranium reduction was selected so that its reactivity decrease would essentially compensate for the reactivity increase obtained from the reduction in the thorium loading. Consequently, the initial core excess reactivity remained essentially unchanged. A summary of the thorium and uranium loadings used in the analysis is given in Table 2.2 for the three different initial core loadings; the old reference loading; a 5% reduced total loading containing 5% less thorium and 6% less uranium; and a 10% reduced total loading containing 10% less thorium and 12% less uranium.

The results of that analysis, which was made primarily with the GAUGE (Ref. 1) diffusion code, are summarized in Table 2.2 and Fig. 2.1. The loading reduction was nearly uniform and the fuel zoning not modified, and therefore the power distribution, as seen in Fig. 2.1, for both the rodded and unrodded cases at the middle of the initial cycle, was essentially unchanged. The initial core excess reactivity for operating conditions as seen for the beginning (BOC) and end (EOC) of the cycle in Table 2.2 is also nearly the same. Since the total control rod worth increased slightly because of the longer diffusion length in the core, the minimum shutdown margin, which is shown in Table 2.2 for the cold clean core with the maximum worth rod pair stuck out, increased by about 0.015 ΔK over the previous design.

The most significant change in the core performance as a result of the loading reduction was a small increase in the age peaking factors and a decrease in the initial core temperature coefficient. There is sufficient margin in the reactor design to accommodate this small increase in power peaking and, since the safety analysis as discussed in the FSAR used an end-of-cycle equilibrium core temperature coefficient that is significantly lower than the reduced initial core temperature coefficient, this latter effect should also be of no consequence.

TABLE 2.2
PRELIMINARY ANALYSIS OF CORE CHARACTERISTICS AND CALCULATED PERFORMANCE
FOR INITIAL CORE WITH REDUCED TOTAL METAL LOADINGS

Core Characteristic	Previous Ref. Load	Reduced 5% Load	Reduced 10% Load
Average C/Th ratio	175.6	185.9	196.7
Average C/U ratio	3,550	3,800	4,060
Total U load, kg	882	825	773
Total Th load, kg	17,800	16,870	15,970
k_{eff} (BOC, hot, clean)	1.071	1.073	1.075
k_{eff} (EOC, hot)	1.014	1.015	1.015
Average conversion ratio, 1st year	0.684	0.682	0.678
Age peaking factor (EOC):			
1st year	1.11	1.11	1.11
2nd year	1.04	1.11	1.22
3rd year	1.29	1.37	1.43
4th year	1.38	1.41	1.47
5th year	1.40	1.45	1.47
6th year	1.30	1.33	1.35
Isothermal temperature coefficient, $10^{-5}/^{\circ}\text{C}$:			
1200°K, BOC	-5.3	---	-5.0
1200°K, EOC	-2.9	---	-2.4
Total control rod worth, Δk	0.235	---	0.247
Maximum worth rod pair, Δk	0.057	---	0.055
Shutdown margin, Max. Rod Out	0.043	---	0.062

Fig. 2.1. Rodded and unrodded radial power distribution at middle of the initial cycle

Because this analysis indicated that neither of the reduced loadings had an adverse affect on the core performance, the core loading with about 10% less metal was selected for the Reference Design 12 nuclear design. The reactivity behavior for that core through the first year of operation was improved by reducing the BOC excess reactivity slightly. This was accomplished by increasing the lumped burnable poison concentration from 0.021 to 0.025 g/cm³ of natural boron in the lumped poison rods. A summary of the reduced uranium and thorium loading for the thirteen initial core compositions for that assembly is presented in Table 2.3, and their locations in the core are given in Fig. 2.2.

Reactivity Analysis for Reference Design 12

Using the reference data previously described and the fuel loading given in Table 2.3, the following problems were run for the Reference Design 12 nuclear analysis at operating temperature:

1. A SCANAL (Ref. 2) rodded depletion.
2. A GAUGE unrodded depletion.
3. A SCANAL unrodded diffusion calculation at BOC and EOC.
4. Seven-group BUGTRI (Ref. 3) unrodded diffusion calculations for the top and bottom core halves at BOC and EOC.
5. A GAMBLE-RZ (Ref. 4) diffusion calculation at BOC and EOC using flux-volume weighted cross sections from the BUGTRI calculations in Item 4.

The GAUGE calculation, which was part of the series of calculations used to select the Reference Design 12 loading, is particularly well suited for these scoping calculations because it is inexpensive and requires little computer time. SCANAL was used to obtain information on the axial power distribution and to obtain a better estimate of the core reactivity throughout life. The GAMBLE-RZ code, which provides the best estimate of the core reactivity when used in conjunction with BUGTRI, was used to normalize the results of the SCANAL calculations.

TABLE 2.3
ADJUSTED FUEL LOADING, INITIAL CORE
(Reference Design 12)

Composition	Location (Refueling Region)	U (kg)	Th (kg)
1	1, 4, 5, 8, 10, 15, 17, 21, 25, 28, 32, 35, 36 - Upper half	105.6	2,905
2	1, 4, 5, 8, 10, 15, 17, 21, 25, 28, 32, 35, 36 - Lower half	80.5	2,596
3	2, 6, 7 - Upper half	39.2	636
4	2, 6, 7 - Lower half	28.9	544
5	9, 11, 12, 14, 16, 19 - Upper half	88.8	1,324
6	9, 11, 12, 14, 16, 19 - Lower half	65.9	1,158
7	20, 23, 24, 26, 27, 30, 31, 34, 37 - Upper half	111.6	1,446
8	20, 23, 24, 26, 27, 30, 31, 34, 37 - Lower half	84.4	1,287
9	3, 13, 18 - Upper half	36.2	720
10	3, 13, 18 - Lower half	25.8	599
11	22, 29, 33 - Upper half	32.1	549
12	22, 29, 33 - Lower half	23.7	474
13	Outer ring of 5 rows of fuel holes and complete control rod element at the core-reflector interface	<u>50.5</u>	<u>1,733</u>
	Total	773.2	15,971

Fig. 2.2. Initial core fuel loading distribution

The results of these analyses (summarized in Table 2.4 and Fig. 2.3) show very good agreement in the prediction of the core reactivity obtained from the different calculations. This good agreement was also observed in previous design analyses (see earlier quarterly report GA-10313) and adds confidence to the values that are calculated for the operating excess reactivity. As seen from the results shown in Fig. 2.3, the excess reactivity generally lies between $0.015 \Delta k$ and $0.022 \Delta k$ for the majority of the cycle, falling slightly below $0.015 \Delta k$ at the end of the cycle. Based on a reactivity uncertainty of $\pm 0.015 \Delta k$, these results indicate an acceptable reactivity behavior for the initial core operation.

TABLE 2.4
GAUGE, SCANAL, AND GAMBLE-RZ UNRODDED REACTIVITY
(Reference Design 12)

	BOC			EOC		
	GAUGE	SCANAL	GAMBLE-RZ ^(a)	GAUGE	SCANAL	GAMBLE-RZ ^(a)
Top core half	---	1.1010	1.1064	---	1.0416	1.0446
Bottom core half	---	1.0602	1.0608	---	1.0173	1.0147
Total core	1.0691	1.0656	1.0683	1.0145	1.0121	1.0121

(a) Upper and lower slice k_{eff} are from the BUGTRI problems that were run to provide flux-volume weighted cross sections for GAMBLE-RZ. EOC BUGTRI concentrations were obtained from SCANAL depletion.

FUEL ZONING AND POWER SHAPING

Calculated Radial and Axial Power Distribution for Reference Design 12

A detailed analysis of the radial and axial power distribution in the reactor during the first year of operation was made using SCANAL, which is a single-channel synthesis depletion code. The adequacy of this analytical method is discussed in some detail in an earlier quarterly report (GA-10202), where the power distributions calculated by means of the SCANAL code are compared with those calculated with a three-dimensional diffusion code. In that comparison, the average axial power distribution and the detailed radial power distribution compared quite favorably.

Fig. 2.3. Reference Design 12 initial core reactivity comparison

The results of the rodded SCANAL depletion are summarized in Table 2.5, and a detailed description of the calculated power distribution is presented in Figs. 2.4, 2.5, 2.6, and 2.7. Because of the presence of two axial fuel zones, the depletion analysis was made for two explicit core slices, so that the top and bottom half of the core each were depleted with the appropriate average flux. This does necessitate anticipating the power split between the two core halves before performing the depletion analysis, but this was no major problem as evidenced by the close agreement between the input and output power fraction for the top and bottom core half shown in Table 2.5 at each time point. The power fraction for the top half of the core ranges from about 67% at the beginning of the cycle to about 53% at the end of the cycle. This shift is caused to a large extent by a bank of partially inserted control rods in the upper half of the core only. The depletion analysis was deliberately made with a bank of partially inserted control rods as a test of the stability of the axial power distribution.

The detailed description for the radial power distribution in the reactor is given in Fig. 2.4 throughout the first year. With the average core power normalized to 1.0, the power factor for each refueling region is shown for each control rod pattern at various times in the initial core cycle. The results show that the maximum power factor from any refueling region is 1.70 at the beginning of the cycle, and this is significantly reduced to below 1.50 for the majority of the cycle. This is substantially below the design limit of 1.83.

The average axial power distribution is shown at various times of the cycle in Figs. 2.5, 2.6, and 2.7 for BOC, 50 days, and EOC, respectively. At each of those times the distribution is compared to the comparable distribution for the previous design (Reference Design 11). Notice that there is very little change from the acceptable axial power distribution of that design. The presence of a partially inserted control rod bank is evident in the results shown in Figs. 2.6 and 2.7. This agrees well with the previously calculated axial power distribution for partially inserted control rods.

Fig. 2.4. Radial power distribution in core throughout first year

Fig. 2.4. (Continued)

TABLE 2.5
DESIGN 12 DEPLETION INPUT AND RESULTS OBTAINED WITH SCANAL CODE

	Core Half	0 EFPD	2 EFPD	2 EFPD(a)	5 EFPD	10 EFPD	10 EFPD(a)	50 EFPD	103.5 EFPD	207 EFPD	310.5 EFPD
Input:											
Rods inserted	Top	13	13	7	7	7	4	4	4	4	4
	Bottom	13	13	7	7	7	1	1	1	1	1
Power fraction	Top	0.6726		0.6546	0.6413		0.6376	0.5456	0.5456	0.5545	0.5505
	Bottom	0.3274		0.3454	0.3587		0.3624	0.4544	0.4544	0.4455	0.4495
Power, MW	Top	566.3		551.2	540.0		536.9	489.4	459.4	466.9	463.5
	Bottom	275.7		290.8	302.0		305.1	382.6	382.6	375.1	378.5
Output:											
k_{eff}	Top	1.0039	1.0039	1.0270	1.0224	1.0179	1.0307	1.0179	1.0189	1.0212	1.0124
	Bottom	0.9918	0.9649	0.9904	0.9865	0.9832	1.0191	1.0105	1.0096	1.0133	1.0101
	FEVER	1.0039	0.9721	0.9953	0.9909	0.9869	1.0073	0.9964	0.9964	0.9989	0.9927
Power fraction	Top	0.6726	0.6546	0.6432	0.6413	0.6376	0.5615	0.5456	0.5545	0.5505	0.5307
	Bottom	0.3274	0.3454	0.3569	0.3587	0.3624	0.4385	0.4544	0.4455	0.4495	0.4692
Power, MW	Top	566.3	551.2	541.5	540.0	536.9	472.8	459.4	466.9	463.5	446.9
	Bottom	275.7	290.8	300.5	302.0	305.1	369.3	382.6	375.1	378.5	395.1
Refueling Regions:											
Control Rods Inserted	Top	1	1	1	1	1	1	1	1	1	1
		11,15,19	11,15,19	11,15,19	11,15,19	11,15,19	11,15,19	11,15,19	11,15,19	11,15,19	11,15,19
		22,28,34	22,28,34	22,28,34	22,28,34	22,28,34					
		20,26,32	20,26,32								
Control Rods Inserted	Bottom	24,30,36	24,30,36								
		1	1	1	1	1	1	1	1	1	1
		11,15,19	11,15,19	11,15,19	11,15,19	11,15,19					
		22,28,34	22,28,34	22,28,34	22,28,34	22,28,34					
Control Rods Inserted	Bottom	20,26,32	20,26,32								
		24,30,36	24,30,36								

(a) Second run indicates rod change.

Fig. 2.5. Rodded axial power distribution at 0 days, 13 rod pairs each in top and bottom half of core

Fig. 2.6. Rodded axial power distribution at 50 days, 4 rod pairs in top and 1 in bottom half of core

Fig. 2.7. Rodded axial power distribution at 310.5 days (EOC), 4 rod pairs in top and 1 in bottom half of core

ANALYTICAL STUDIES

Analysis of Some Peach Bottom Experimental Results

To enhance the confidence level of the calculated data for the reactor, some analysis was made on Peach Bottom experimental results in which an apparent discrepancy previously existed between the measured and analytical results. The analysis concentrated (1) on the reactivity worth of the equilibrated xenon and the temperature defect between room and operating temperature as measured in Peach Bottom Core 2 and (2) on the count rate of the source-range detectors as measured during the startup of the initial Peach Bottom core. It used analytical methods and cross-section data presently being used in the Fort St. Vrain reactor core analysis.

Xenon Reactivity Worth. It is difficult to determine accurately the worth of equilibrated xenon for Peach Bottom, because a direct measurement of it was not made during the Core 2 startup tests. During the approach to full power while the equilibrium xenon concentration was being generated, the temperature of the core and the number of inserted control rods varied. Consequently, a more accurate measurement was made for the combined worth of the temperature defect and the equilibrated xenon. A detailed description of these experimental results, including a discussion of the methods, is given in Ref. 5. An attempt was made to separate the two effects, so that the calculated worth of each could be compared directly to a specific measured value.

The calculated data for the core, critical at room and operating temperatures, is given in Table 2.6. The worth of xenon in the hot critical core lies between 0.0222 and 0.0230 $\Delta\rho$, with an average worth of 0.0226 $\Delta\rho$ (see Table 2.7). This is in good agreement with the quoted measured value of 0.0233 $\Delta\rho$, but it must be remembered that this value was not obtained directly. However, the calculated temperature defect which lies between 0.065 and 0.075 $\Delta\rho$ and has an average value of 0.070 $\Delta\rho$ also agrees well with the measured value of 0.071 $\Delta\rho$, so that the calculated worth of the combined effect would appear to be in good agreement with the measured value of the combined effect.

TABLE 2.6
CALCULATED k_{eff} FOR VARIOUS PEACH BOTTOM CORE 2 CONDITIONS

Case	Temp. (°K)	No. of Rods (a)	Xenon	k_{eff}
1	1200	A	No	0.9939
2	1200	A	Yes	0.9716
3	300	A	No	1.0627
4	1200	B	Yes	0.9942
5	1200	B	No	1.0167
6	300	C	No	0.9958
7	300	C	Yes	0.9690
8	1200	C	No	0.9263

(a) Identifies a set of consistent calculations obtained with a like number of rods.

TABLE 2.7
XENON AND TEMPERATURE REACTIVITY WORTH OF PEACH BOTTOM CORE 2
(Refer to Table 2.6)

Cases	Xe-135 Worth		Cases	Temp. Defect	
	Δk	$\Delta \rho$		Δk	$\Delta \rho$
1-2	0.0233	0.0233	1-3	0.0688	0.0650
4-5	0.0225	0.0222	---	---	---
6-7	0.0268	0.0277	6-8	0.0695	0.0752
Worth:					
Avg Calc	---	0.0226	---	---	0.070
Measured	---	0.0233	---	---	0.071

Source Calculation for Core 1. In the original analysis for the expected detector count rate from the reactor startup sources located in Peach Bottom Core 1, the calculated count rate overestimated the measured count rate data by at least one order of magnitude (see Ref. 6). Since that analysis was performed, there have become available improved analytical methods involving improved transport-theory codes as well as various diffusion theory codes with external source options. To check the adequacy of these methods in predicting the detector count rate from the startup sources in the Fort St. Vrain reactor, a reanalysis has been made of those experimental results. The configuration showing the relative source and detector location for that analysis is shown in Fig. 2.8.

The attenuation of flux from a source to a detector was calculated in a cylindrical geometry by a one-dimensional, transport-theory method code. The assumptions implicit in the method are:

1. The source and detector are in the same plane.
2. The source and detector lie on the same radial vector.
3. The source is infinitely high and uniform in the azimuthal direction.

Because of these assumptions the calculated flux at the detector will be overestimated. Since the actual sources are finite in height and azimuthally discrete, adjustment factors have to be applied to the results of the transport theory calculation. The necessary adjustment factors are defined as follows:

1. The height adjustment factor (HAF) is a ratio of the flux at a detector elevation from a finite source annulus to the flux from an infinitely high source annulus.
2. The azimuthal adjustment factor (AAF) is a ratio of the flux at a detector vector from a discrete source to the flux from a uniform source annulus.

The best estimate of the flux at a detector is therefore defined as

$$\Phi_D = \Phi_T \times \text{HAF} \times \text{AAF} \quad ,$$

Fig. 2.8. Peach Bottom reactor core showing relative source and detector locations

where Φ_T is the flux calculated by a transport-theory code.

Assuming that deep in the side reflector the fast flux ($E > 0.18$ MeV) shape, in axial and azimuthal directions, is essentially the same as at the detector location, the adjustment factors can be easily obtained from two-dimensional, diffusion-theory calculations.

The results of that analysis are summarized in Table 2.8. For the fully loaded core with all control rods inserted at room temperature ($k = 0.945$), the calculated count rates agree quite well with those measured. From the analytical data, it is possible to predict the expected count rate from a core with a higher multiplication ($k = 0.964$), and this also agrees quite well with that measured. This indicates that by using the more refined analytical methods now available, it is possible to adequately predict the detector response to the startup neutron sources.

Neutron Sources Required for Reactor Startup

Two Cf-252 neutron sources have been ordered for use in the initial reactor startup operations. Consistent with the calculated requirements for these sources as discussed in the previous quarterly report (GA-10444), each source will have a yield of 1.75×10^9 n/sec. They will be placed in the top layer of fuel elements about two-thirds of the way out on a core radius on opposite sides of the core in the regions directly below the two startup detectors in the top of the PCRV. They will be placed in fuel elements which will not be refueled until after five and six years of operation. In addition, six unirradiated Sb-Be sources will be located in the reflector region around the core for use as sustaining neutron sources. The size of these sources is based on the requirement that five cps be obtained on the startup-range detectors with all control rods inserted after 120 days of core shutdown.

TABLE 2.8
COUNT RATE AT DETECTORS

	SR I	SR II
θ_{eff} (a)	500 nv	500 nv
Height adjustment factor	0.085	0.085
Azimuthal adjustment factor	0.006	0.030
Overall adjustment factor	5.1×10^{-4}	2.5×10^{-3}
$\theta_{\text{eff}}/\text{detector}$	0.255 nv	1.25 nv
Detector sensitivity, cps/nv	40	4
Calculated detector, C.R.	~ 10	5
Measured detector, C.R.	7	5
Number of direct C.R.	0	0.5
Expected C.R. at $k = 0.964$ (b)	15	7.3
Measured C.R. at $k = 0.964$	14	8

(a) Transport-theory calculation.

(b) At $k = 0.945$, $Q_f = 18.2$; at $k = 0.964$, $Q_f = 27.8$;
fission source increase = 1.50.

Reactivity Worth of the Reserve Shutdown System

It is required that the reserve shutdown system shut the reactor down to refueling temperature from any hot operating or hot standby condition. In addition, the first group of seven to be released must introduce a negative reactivity worth of at least $0.015 \Delta k$ with any one of the seven channels inoperative.

The reactivity worth of the reserve shutdown system was calculated for the Reference Design 11 of the initial core. The reactivity worth of all 37 channels was calculated to be $0.139 \Delta k$ in the initial core and $0.130 \Delta k$ for the equilibrium core. Even with the two maximum worth channels inoperative, the worth is still about $0.093 \Delta k$, which is sufficient to shut the reactor down to refueling temperature with xenon decay. In addition, the designated seven-hopper subset was calculated to have a worth greater than $0.015 \Delta k$ with the maximum worth channel inoperative. Because of the longer neutron diffusing length in the lighter loaded Reference Design 12 core, the reserve shutdown system should be worth even more for that assembly.

For the sake of simplicity, the analysis was made exclusively for the unrodded core, even though the reserve shutdown system would generally be activated when the core is in a just-critical condition with some control rods either partially or fully inserted. However, the largest reactivity defect and consequently largest reactivity requirement for the reserve shutdown system exists at the full-power, equilibrium poisoned condition when most of the control rods have been withdrawn. The analysis was made using the GAUGE diffusion code, and both the reactivity requirements and the calculated worth of the reserve shutdown system are summarized in Table 2.9.

Source Power Rod Withdrawal Accident

The greatest reactivity insertion and the largest credible reactivity insertion rate in the reactor result from the accidental withdrawal of control poison. For a rod withdrawal accident from source power, an artificial core configuration and an abnormal withdrawal sequence leads to an

TABLE 2.9
OPERATING REACTIVITY DEFECT AND RESERVE SHUTDOWN
SYSTEM REQUIREMENTS (Δk)

	Initial Core			Equilibrium Core MOC
	BOC	MOC	EOC	
Temp defect (no xenon):				
To 300°F	0.055	0.051	0.044	0.040
To 400°F	0.049	0.045	0.039	0.035
Xenon decay	0.030	0.031	0.032	0.032
Pa-233 decay	0.026	0.027	0.028	0.026
Samarium buildup	-0.004	-0.004	-0.004	-0.004
Max. control rod worth at operating temp	0.015	0.015	0.015	0.015
Reserve Shutdown Reactivity Requirements				
1. Shutdown to 400°F	0.049	0.045	0.039	0.035
2. Shutdown to 300°F	0.055	0.051	0.044	0.040
3. SD to 400°, xenon decay	0.079	0.076	0.071	0.067
4. SD to 300°, xenon decay	0.085	0.082	0.076	0.072
5. SD to 300°, Xe and Pa decay	0.107	0.105	0.100	0.094
6. Shutdown to 400°F during rod withdrawal	0.064	0.060	0.054	0.050
7. Shutdown to 300°F during rod withdrawal	0.070	0.066	0.059	0.055
Calculated Worth for Bank of Reserve Shutdown Rods				
	BOC Initial Core	EOC Equilibrium Core		
k_{eff} , unrodded, hot operating	1.070	1.004		
k_{eff} , 37 RS channels in	0.931	0.874		
k_{eff} , 36 RS channels in, No. 30 out	0.950	---		
k_{eff} , 36 RS channels in, No. 31 out	0.950	---		
k_{eff} , 35 RS channels in, Nos. 30 and 31 out	0.977	0.915		
Worth of RSS (all 37 channels), Δk	0.139	0.130		
Worth of RSS (36 channels), Δk	0.120	---		
Worth of RSS (35 channels), Δk	0.093	0.089		

accident that produces an average fuel temperature rise of about 500°C and a maximum fuel temperature rise of about 2500°C, as described in section 14.2.2 of the FSAR. This source power rod withdrawal accident was reevaluated to eliminate some of the conservatism in the original analysis. The accident results are affected by the selection of core parameters, such as maximum rod worth, minimum scram reactivity available, and temperature coefficient to be used in the analysis. In an attempt to keep the model simple for analytical reasons, and maintain conservatism, parameters were selected in the original study that were somewhat inconsistent with the core conditions.

After reviewing the calculated and expected excess reactivity and control rod worth for the core, it was decided that the minimum shutdown margin as reported in the FSAR is still adequate. However, some additional detail regarding the worth of the control rod being withdrawn to obtain values consistent with core temperature was necessary. This was done, and kinetic analysis was made for the reactor at source power starting from both room and refueling temperature using values for the worth of the control rod being withdrawn and the available scram reactivity that are consistent with these temperatures. A significant reduction in the core temperatures during the accident was obtained: an average fuel temperature rise of about 300°C and a maximum fuel temperature rise in the hottest channel of about 1600°C. Pertinent data are shown in Fig. 2.9 and Table 2.10. Under these temperature transients, very little particle failure would be expected. This model still contained the simplifying and conservative assumption that the reactivity feedback is based on the average core temperature, and no temperature feedback is used in calculating the peaking factors used in the hot-channel analysis. Consequently, the FSAR was modified to reflect this consistent and more realistic description of the rod withdrawal accident from source power.

REFERENCES

1. Wagner, M. R., "GAUGE, A Two Dimensional Few Group Neutron Diffusion Depletion Program for a Uniform Triangular Mesh," USAEC Report GA-8307, Gulf General Atomic, March 15, 1968.

Fig. 2.9. Rod withdrawal accident from source power

TABLE 2.10
SUMMARY OF ROD WITHDRAWAL ACCIDENT FROM SOURCE
POWER WITH DIFFERENT CORE PARAMETERS

	Reference Case From FSAR	Room Temp Rod Withdrawal	Refueling Temp Consistent Rod Worth
Control rod worth withdrawn, Δk	0.047	0.047	0.034
Scram worth, Δk	0.058	0.058	0.063
Initial core temperature, °C	200	27	200
Flow rate, % of full flow	10	10	10
Region power peaking factor:			
Before	1.25	4.60	1.25
After	9.95	9.95	9.95
Energy release, MW-sec	12.35 + 4	5.65 + 4	5.1 + 4
Maximum power level, MW	2550	1736	1723
Avg fuel temp rise, °C	530	320	245
Avg fuel rod temp, °C	730	345	445
Avg fuel rod (max. temp), °C	950	455	525
Avg gas temp (3 min), °F	675	320	545
Hot channel fuel ΔT , °C	2575	1600	1100
Hot channel fuel temp, °C	2775	1625	1300
Hot channel (max. temp), °C	4190 ^(a)	2200 ^(a)	1800 ^(a)
Hot channel gas out, °F	1450	1000	720

(a) About 1% of the fuel in the hot channel is at this temperature. For the second case, about 10% of the fuel is above 2000°. The hot channel constitutes one refueling region, or about 2.7% of all the fuel in the core.

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3. Dorsey, J. P., et al., "BUG-2/BUGTRI, Two Dimensional Multigroup Burnup Codes for Rectangular and Hexagonal Geometry," USAEC Report GA-8272, Gulf General Atomic, August 22, 1969.
4. Dorsey, J. P., et al., "GAMBLE-5, A Program for the Solution of the Multigroup Neutron Diffusion Equation in Two Dimensions with Arbitrary Group Scattering for the UNIVAC-1108 Computer," USAEC Report GA-8188, Gulf General Atomic, December 4, 1967.
5. "40-MW(e) Prototype High-temperature Gas-cooled Reactor Postconstruction Research and Development Program, Quarterly Progress Report for the Period Ending October 31, 1970," USAEC Report GA-10400, Gulf General Atomic, November 30, 1970.
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TASK III
FUEL DEVELOPMENT

MATERIALS

The objectives of this subtask are to:

1. Determine the characteristics of beds of coated particles by means of fabrication technique investigations, thermal property tests, vibration and impact tests, and measurements of packing density and loading uniformity.
2. Select the type of particle bed to be used in the reactor based on the above work and work carried out in other parts of the program, taking into account the expected characteristics, including reprocessing, and determine the suitability of the selected system by means of further tests of the type described above.
3. Determine the characteristics of various candidate graphites for use in the fuel elements by means of measurements of the physical, thermal, and mechanical properties of these graphites.
4. Determine the characteristics of various control materials by means of measurements of physical, mechanical, chemical, and thermal properties.
5. Determine the irradiation stability of candidate graphites for use in the fuel elements by means of irradiation tests conducted in the Engineering Test Reactor at the temperatures and to the integrated fast flux expected in the plant, followed by postirradiation examination.

6. Establish the effect of fast flux on fuel particle coating structural integrity as a function of coating structure and exposure by means of irradiation tests in the Engineering Test Reactor and other reactors on unfueled samples of particle coatings; postirradiation measurements of coating properties such as dimensional changes, flexural strength, and crystallite spacing; and subsequent reirradiation to design integrated fast flux and remeasurement. These irradiations will be carried out in the graphite capsules used in 5 above, and in the coated particle capsules used in 7 below.
7. Determine the irradiation stability of coated fuel particles for use in the fuel elements by means of a series of (1) short-term tests on coated fuel particles to select a coated fuel particle (or particles) having over its design life a high degree of structural integrity, followed by (2) longer-term irradiation tests conducted at design temperature, to design burnup, and to a significant fraction of the integrated fast flux expected in the plant. The "significant" integrated fast dosage will be determined from the results obtained in 6 above. Capsule tests will be followed by postirradiation examination of the particles. Approximately seven short-term and three longer-term capsules are planned for this part of the program.

Control Materials

The objective of this work is to select and characterize control and poison material for use in the PSC core. The control and poison material considered here are (1) control rod absorbers, (2) reserve shutdown balls, (3) burnable poison rods, and (4) boronated graphite pellets for the lower reflectors.

Temporary Poison Rods. The temporary poison rods will consist of metal cans filled with boron carbide. They will be placed in the control rod and reserve shutdown holes during initial loading of the core and will be removed when loading has been completed. A specification was written to describe the boron carbide granules used to fill the metal container.

Reserve Shutdown System. Forty pounds of nominal 9/16-in.-diameter boronated graphite spheres were received at Gulf General Atomic from Union Carbide as a sample from a preproduction run. The spheres, which were manufactured by a cold-press-and-bake technique, consist of B_4C granules in a matrix of graphite and carbonized pitch binder. A typical microstructure of one of the sample spheres is shown in Fig. 3.1. The density of a total of 40 spheres was determined by water displacement techniques to be 1.76 ± 0.05 g/cm³, well above the 1.70 g/cm³ minimum specified density. However, chemical analysis for boron showed the boron content to be lower than required by the specification. The measured boron content of four spheres was 35 wt-%, which would produce a boron density of 0.62 g B/cm³ as opposed to the specified minimum of 0.63 g B/cm³. The vendor has been notified of this discrepancy and appropriate action is being taken.

The thermal shock resistance of the balls was of interest because fragmentation and dusting must be minimized in the event they are released into the core during reactor operation. In order to provide assurance that the spheres had sufficient resistance to thermal shock, an experiment was conducted to test their resistance. A 3-in.-diameter, 6-in.-long, closed-end steel container was fabricated with a 5/8-in. opening in the top end, and a Chromel-Alumel thermocouple was attached to the side of the container at the bottom end. A 1/4-in.-diameter steel tube introduced argon purge gas into the container at the bottom. The assembly was placed in a 3-in.-diameter vertical clam shell resistance heated furnace with a purge gas flow of about 800 cm³/min. The assembly was heated until the thermocouple indicated a can temperature of 1000°C, which is representative of the highest core temperature the spheres would be exposed to under service conditions.

M34584-1

500X

Fig. 3.1. Typical microstructure of reserve shutdown sphere boronated graphite containing 0.62 g B/cm^3 (35 wt-% B)

Twenty boronated graphite spheres weighing a total of 48.1765 g were then dropped one at a time into the purged can from a point 25 in. above the bottom of the heated can. The can was then cooled, the spheres were removed, and the weight of the spheres was determined. The experiment was then repeated in the same manner. The weight loss of the spheres was 0.065 wt-% during the first drop and 0.002 wt-% during the second drop for a total weight loss of 0.067 wt-%. This amount is well below the maximum permissible total dust production of 0.5 wt-% required by the specification. The appearance of the spheres was unchanged by the thermal shock test. A photograph of the spheres after the second thermal shock is shown in Fig. 3.2.

Burnable Poison. Preproduction runs have been made at Great Lakes Carbon Company. There is extremely good distribution of B_4C granules in the current material as revealed by metallography at Gulf General Atomic.

Oxidation of B_4C in Burnable Poison Rods. Experiments on the oxidation of B_4C in a burnable poison rod were performed, and the results were used in calculating boron oxidation rates in the burnable poison rods in the reactor. It is concluded from this work that B_4C oxidation is water-transport limited under normal reactor conditions (1300° to 1800°F, 1 ppm by volume water, and 50 atm helium pressure) and that during normal reactor operation B_4C will oxidize at an acceptably low rate of less than 10%/yr. During normal startup or refueling operations where graphite temperatures around 400°F are involved, the B_4C oxidation rate will be negligibly low. In the event of a steam inleakage accident, the oxidation rate would be negligibly low because the reactor would be rapidly scrammed and the core temperature would be low (around 600°F) during cleanup. The experimental and analytical work leading to these conclusions is described below.

A burnable poison rod was exposed to water vapor in helium at temperatures ranging from 400° to 800°C (752° to 1472°F) and at water vapor pressures of 0.0003, 0.003, 0.006, and 0.03 atm in a total helium pressure of 1 atm. These partial pressures correspond to 6, 60, 120, and 600 ppm by volume water in the reactor at 50 atm helium, respectively.

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Fig. 3.2. Boronated graphite reserve shutdown spheres after two consecutive thermal shocks from room temperature to 1000°C . There was a weight loss of 0.067 wt-% due to some dusting, but no fragmentation or chipping occurred

The rod, which consisted of B_4C particles ($\leq 20 \mu\text{m}$ diam) in a graphite matrix, was made by hot pressing and annealing. The rod contained 0.083 g B/cm^3 (or 5.16% B), which corresponds closely to the rods in the Fort St. Vrain reactor containing the highest boron content, 0.1 g B/cm^3 . The rod was 0.62 in. in diameter and 0.67 in. long. The rods in the reactor will be 0.42 in. in diameter by about 6 in. long.

The reaction rate was monitored on a semicontinuous basis by analyzing the effluent gas for hydrogen. The reaction is

Thus, for each g-atom of boron oxidized, 1.5 g-atoms of hydrogen are produced. At the end of the test, analysis of the sample for both B_4C and B_2O_3 provided an additional measurement (verification) of the boron oxidation rate.

The experimental results are shown in Fig. 3.3, where the boron oxidation rate in percent per hour, as determined by hydrogen analyses, is plotted versus $1/T$ for three water partial pressures. The experimental data are reasonably well represented by the equation

$$B_E = 2.23 \times 10^5 C_{H_2O}^{0.6} e^{-\frac{19.4 \times 10^3}{RT}} \quad , \quad (1)$$

where B_E = experimentally observed boron oxidation rate, %/hr,

C_{H_2O} = water partial pressure, atm,

T = temperature, $^{\circ}\text{K}$, and

R = the gas constant, 1.987.

At the conclusion of the test series, the specimen was pulverized and analyzed for both total boron and leachable boron (B_2O_3). This analysis showed that 28% of the boron in the sample had been converted to B_2O_3 ,

Fig. 3.3. Oxidation rate of B_4C in boronated graphite, $B_4C + 6H_2O = 2B_2O_3 + 6H_2 + C$. Sample contains 5.16% B in graphite, B_4C particles $\leq 20 \mu\text{m}$. Specimen is 0.5 in. in diameter x 0.6 in. long. Total gas pressure is $\sim 1 \text{ atm He}$

which is in close agreement with total boron oxidation calculated from hydrogen evolution.

Almost none of the B_2O_3 volatilized as boric acid during the experiments. This was expected because volatile boric acid (HBO_3) does not form at the temperatures used in the experiments, 400° to 800°C (752° to 1472°F). Boric acid forms and volatilizes only at 300°C (572°F) or below, such as might occur during a reactor shutdown following a steam ingress situation.

The experimental data given in Fig. 3.3 have been applied to the burnable poison rods in the reactor using the following relationship:

$$B_R = B_E \frac{A_E}{A_R} \frac{1}{\sqrt{P_{He}}} \quad , \quad (2)$$

where B_R = boron oxidation rate at reactor conditions, %/hr,

A_E = surface area of experimental rod, cm^2 ,

A_R = surface area of reactor rod, cm^2 , and

P_{He} = helium pressure in reactor, atm.

The correction factor for He pressure ($1/\sqrt{P_{He}}$) in this relationship is based on equations developed by Crank (Ref. 1) and Zumwalt (Ref. 2), which show that gas-solid reactions in a porous medium vary with $\sqrt{D}$, where D, the diffusion coefficient of water in helium, is a function of $1/P$.

Presented below are methods for calculating the degree of water vapor transport through the graphite web to the burnable poison rods via diffusion and permeation flow.

Calculations of transport via diffusion can be made with the following relationship, assuming that all of the water vapor that diffuses in reacts with B_4C thereby creating the necessary concentration gradient:

$$n = \frac{ZA P_{H_2O}}{RTL} \quad , \quad (3)$$

where n = flow of water vapor, g-mole/sec,

A = area of equivalent cylinder around burnable poison hole, cm^2/cm of length,

$P_{\text{H}_2\text{O}}$ = water pressure, atm,

R = gas constant, $82 \text{ cm}^3 \text{ atm deg}^{-1} \text{ mole}^{-1}$,

T = temperature, $^{\circ}\text{K}$,

L = web thickness of equivalent cylinder, cm, and

A = water diffusion coefficient in graphite.

The values used for A and L are $6.5 \text{ cm}^2/\text{cm}$ of length and 1.3 cm , respectively.

The factor Z is defined by the relationship

$$Z = \frac{m D_{1,2}}{P_{\text{He}}},$$

where $D_{1,2}$ = interdiffusion coefficient for water in helium, cm^2/sec ,

P_{He} = total helium pressure, atm, and

m = a geometry factor (porosity/tortuosity) that relates ordinary gas-phase diffusion to diffusion in a porous solid.

The value used for m is 0.01, which is considered conservative for H-327 graphite.

An estimation of the degree of water vapor transport via permeation flow is made using the equation

$$W_F = \frac{\beta \rho K_{\mu} A \Delta P}{\mu_1 L}, \quad (4)$$

where W_F = flow of water vapor, g-moles $\text{H}_2\text{O}/\text{sec}$,

β = mole fraction of water in helium,

ρ = helium density, $\text{g-mole}/\text{cm}^3$,

K = permeability coefficient at 1 atm, cm^2/sec ,
 μ_0/μ_1 = ratio of helium viscosity at room temperature to that at reactor temperature,
 A = area of equivalent cylinder around burnable poison hole, cm^2 ,
 ΔP = pressure drop across the web, atm, and
 L = thickness of equivalent web, cm.

In applying Eq. (4) to the FSV reactor, the value used for K is $1.2 \text{ cm}^2/\text{sec}$, which is the average of several measurements on H-327 graphite with the flow perpendicular to the extrusion direction (Ref. 3). The value used for A is 228 cm^2 , which is one-half the total area, since inward flow occurs only in half of the fuel element. ΔP is the average pressure drop across half of the fuel element averaged over one year of reactor operation, that is, the total fuel element pressure drop divided by 4. The ΔP value used (7.7×10^{-3} atm) is for the equilibrium core. The ΔP value for a new core is about 5.6×10^{-3} atm. The equivalent web thickness, L , was taken to be 1.3 cm.

The model for Eq. (4) is flow of helium through the web into the burnable poison channel in the top half of the fuel element and out in the bottom half of the element. The equation is similar to one given by Ozisik and Silverman (Ref. 4) in their analysis of permeation (transverse) flow in graphite fuel elements.

The oxidation rate of boron in the FSV burnable poison rods was calculated for various reactor conditions of temperature and water concentrations. The results are shown in Fig. 3.4. The solid lines represent chemical reaction rate data calculated using Eqs. (1) and (2), assuming that the mass transport of water is not limiting. The dashed lines, calculated using Eqs. (3) and (4), show how the oxidation rate is limited by diffusion and permeation flow through the graphite web. The diffusion and permeation flow calculations were made assuming conservatively that water is not depleted by reaction with graphite, but reacts quantitatively with B_4C in the burnable poison rod. It can be seen in Fig. 3.4 that under conditions where the oxidation rate is transport controlled, the rate varies little with temperature.

Fig. 3.4. Oxidation rate of B₄C in burnable poison rods

Predicted boron oxidation rates at reactor conditions for three cases of interest are given in Table 3.1. Each of these cases is discussed below.

For Case 1 (normal reactor operation), the technical specifications allow 10 ppm by volume total oxidants (CO , CO_2 , H_2O) during normal reactor operation. It is expected that the water content will be about 1 ppm by volume, or 5×10^{-5} atm, at the normal reactor pressure of 50 atm. The temperature of the burnable poison rods and adjacent graphite will be in the range of 700° to 1000°C (1292° to 1832°F). Figure 3.4 shows that under these conditions the oxidation rate is controlled by water transport and that the predicted rate of B_4C oxidation is 2.5×10^{-2} %/day, or about 10%/year. For this case, the level of transport of water by permeation is calculated to be about twice that by diffusion. The value of 10%/year is acceptably low, on the basis of an analysis by Marshall (see Ref. 5 and the discussion below).

For Case 2 (steam inleakage accident) with a single offset tube failure with normal protective action, approximately 400 lb of water vapor will leak into the main coolant. The reactor will be automatically scrammed, and the core will cool down within minutes to 300°C (572°F) or lower. The steam partial pressure will be initially 0.4 atm, but the steam will be removed from the system by the purification system at a rate of about 12%/hr. Thus, in 50 hr, the steam partial pressure would be reduced to less than 10^{-3} atm. The average water concentration during this 50-hr period is 0.1 atm.

Since the core graphite temperature is low during the period of cleanup and water vapor pressures are high, the level of water transport will be high, and B_4C oxidation will be controlled by the rate of chemical reaction (see Fig. 3.4). Calculations show that during a 100-hour period at 0.1 atm H_2O vapor only about 2×10^{-2} percent of the B_4C in the burnable poison will oxidize. This estimate was made assuming a steady temperature of 300°C (572°F) during the cleanup operation. In actual operation, however, much lower temperatures (around 400°F) would likely be achieved during cleanup, and this would further minimize the level of B_4C oxidation.

TABLE 3.1
PREDICTED MAXIMUM OXIDATION RATE OF B_4C IN BURNABLE POISON RODS

Case	Press. (atm)	H ₂ O Content		Temp		B_4C Oxidation Rate (%/day)
		(ppmv)	(atm)	(°C)	(°F)	
1. Normal	50	1	5×10^{-5}	700– 1000	1292– 1832	$2.5 \times 10^{-2(a)}$
2. Steam leak	25	2000	0.05	300	572	$4.6 \times 10^{-3(b)}$
3. Startup	25	1000	0.025	200	392	$1.7 \times 10^{-4(b)}$

(a) For 1 ppmv water and temperatures above 1000°F, B_4C oxidation in the poison rods is water-transport controlled (see Fig. 3.4 and text).

(b) Reaction rate limited.

For Case 3 (reactor startup), it is expected that moisture levels will not exceed 100 ppm by volume. This relatively low level of moisture is expected because (1) most of the reactor components including reflector graphite and fibrous insulation will be fully outgassed during the hot runs prior to installation of the active core and (2) the initial water content of the active core is expected to be 30 lb or less. Thirty pounds of water instantaneously released into the main loop helium would result in a water content of about 1000 ppm by volume. However, since core outgassing will occur over a long period of time and since the purification system will be in operation during startup, the water concentration should not exceed about 100 ppm by volume. Furthermore, water removal from the active core should be largely complete at a temperature of 300° to 400°F. Thus, blower heat alone should effectively degas the core before nuclear startup. Calculations show that for the very pessimistic case of 1000 ppm by volume water at 392°F during startup, a B_4C oxidation rate of less than 2×10^{-4} %/day would be expected. Therefore, for a normal startup, where water levels of less than 100 ppm by volume are encountered, the degree of boron oxidation should be negligibly low.

Studies have shown that a substantial transport of burnable poison could be tolerated in the Fort St. Vrain reactor before significant changes in nuclear reactivity would occur. It is estimated that a loss of 30% boron per year is tolerable, because the B-10 burnup rate ($t^{1/2} \approx 100$ days) is much faster than the boron oxidation rate. For an oxidation rate of 30%/year or 2.5%/month, it is calculated that the maximum B-10 loss will be only 5% of the original B-10 charge, which is acceptably low. Furthermore, calculations indicate that at the predicted B_4C oxidation rate of 10%/year under normal operation, the maximum B-10 loss will be only about 1.7%. On this basis, oxidation and transport of boron during normal reactor operation is not a problem.

The estimates of water vapor transport given above are believed to be conservative for the following reasons:

1. In the calculations it was assumed that water is not depleted by reactions with graphite. In approximately one-half of the core, graphite temperatures around the burnable poison rods will exceed 900°C (1660°F). At this temperature the steam-graphite reaction rate is appreciable and will significantly deplete the flow of water vapor through the graphite.
2. The temperature of the poison rods in approximately one-half the core exceeds 1038°C (1900°F). At this temperature much of the water vapor that penetrates the web will react with the carbon matrix of the burnable poison rod causing a further depletion of the oxidant.
3. The transport calculations [using Eqs. (3) and (4)] utilize a helium permeability coefficient, K , and a diffusion coefficient, Z , that are characteristic of H-327 graphite. The values used for these two constants were obtained by averaging several measurements (with flow perpendicular to the extrusion direction) on samples of H-327 graphite taken from various positions from several logs (Ref. 3). It is known, however, that the outer edge of a graphite log is more dense, and thus less permeable, than the rest of the material. Since burnable poison holes will be located in the extreme outer edge of the fuel block, it is possible that the true permeability and diffusion coefficients will be somewhat lower than the average values used. Thus, the water transport rate through graphite near the outer edge of the log should be substantially less than that of the rest of the material.
4. The pressure drop used in the permeation flow calculation [using Eq. (4)] is the pressure drop averaged over one year of reactor operation for a new fueled region in an equilibrium core. An analysis by McEachern (Ref. 6) shows that the actual core pressure drop during this period increases from 0.33 to 0.57 psi/block after 1 year of operation. The value used in Eq. (4) was 0.45 psi/block,

i.e., the yearly average. Moreover, the expected average pressure drop for the initial core loading is 0.33 psi/block. Thus, permeation flow for the initial core is expected to be about three-fourths that of the equilibrium core.

It is concluded from this study that the degree of oxidation of B_4C in the FSV burnable poison rods is acceptable low (1) under normal reactor operation, (2) under startup conditions, and (3) under the conditions of a steam inleakage accident.

Graphite Development

The fuel elements and replaceable reflectors are manufactured from logs of grade H-327 graphite produced by Great Lakes Carbon Corporation. Each log is approximately 18 in. in diameter and 34 in. long and is the product of the batch manufacturing process of mixing, extrusion, baking, and graphitizing. The characteristics of strength and purity of a given blend of material are mainly the result of controlled heat-treatment procedures during which the graphite logs are chemically purified and graphitized. The heat-treatment batch size scheduled for the fuel elements averaged 25 logs per furnace for the first 50 heat-treatment batches produced between March and August 1969. Heat treatment of the remainder of an approximately equal amount of core graphite recommenced in the latter half of 1970. In addition to the standard procedure of chemically analyzing aliquot samples of the center and edges of 10% to 20% of the logs in each furnace charge, each log in the first three graphitization runs was analyzed for ash, boron, iron, titanium, and vanadium. All the logs produced are tested for tensile strength in the parallel to extrusion direction by coring a sample from a position in the center section of the log coincident with the fuel element handling tool access hole. This access hole extends just under halfway into the 31-in. length of the element and the tensile test sample is machined from the first 9 in. of core length. From the results of the chemical and mechanical tests, the graphite logs are graded for use in the core, the principal fuel block grade being required to have less than 1000 ppm ash, 100 ppm iron, 50 ppm vanadium and titanium, and a total boron equivalent of

not more than 5 ppm. The minimum specified strength of the fuel block test material is 1000 psi parallel to the extrusion direction.

In addition to the above quality control tests, logs approved for machining into fuel element blocks have been selected at intervals in the production schedules for further chemical analysis and have been measured for variations in the structure and tensile mechanical strength throughout the full section of the log. Tests on samples of material from the penultimate stage in the manufacture of PSC grade A graphite logs are summarized as follows:

The amount of common impurities in PSC fuel block logs selected at random during the production of the first half of the core are shown in Table 3.2. In this series of tests the log location with the largest boron content was shown to be the end-edge position of the log, and the majority of the analytical samples were selected from this location. The range in the common metal impurity levels is shown to be within the statistical levels established by analysis of all logs in the first three graphitization furnace charges (see Table 3.3). This is also true when comparing the composition of a log selected at the mid-stage of production. Analyses of the iron, vanadium, and titanium content of logs selected at the penultimate stage of the production schedule show increases in these metal impurities. However the impurity content remains well within the specified limit for grade A fuel block materials and the increases are thought due to acceptable production process variations, particularly at the purification stage.

In a continuing assessment of the strength of grade A graphite logs produced at different periods during the production schedule, two logs from the late stages of manufacture have been tensile tested. Previous work had established that the biggest differences in tensile strength within a log are across the section at the end and mid-length. The current test data (see Tables 3.4 and 3.5) shows the following:

TABLE 3.2
TYPICAL COMMON ELEMENTAL IMPURITY LEVELS FOR GRADE H-327 GRAPHITE
(Spectrochemical Analysis by GGA unless otherwise stated)(a)

Log No.	Position	Element Concentration (ppm) ^(b)									
		Al	B	Cu	Fe	Mg	Si	Ti	V	Mn	Ash ^(c)
10-2/694	End Edge	1.0	ND	ND	ND	2.0	<10.0	4.0	ND	ND	21
3-1/705	End Edge	1.0	1.0	ND	ND	2.0	<10.0	8.0	ND	ND	38
2-2/739	End Edge	1.0	2.0	ND	ND	2.0	10.0	10.0	ND	ND	55
7-2/744	End edge	4.0	2.0	ND	2.0	6.0	20.0	8.0	ND	ND	77
12-2/769	End edge	1.0	1.0	1.0	1.0	4.0	20.0	10.0	ND	ND	55
12-2/956	End Edge	4.0	1.0	1.0	2.0	4.0	20.0	10.0	ND	ND	472
12-1/958	End Edge	ND	1.0	ND	2.0	1.0	20.0	20.0	ND	ND	317
8-2/1058	End Edge	1.0	1.0	ND	ND	4.0	20.0	10.0	ND	ND	72
4-2/1959	End Edge	6.0	ND	1.0	10.0	10.0	80.0	10.0	ND	ND	77
4-2/2479	End Edge	4.0	1.0	1.0	1.0	6.0	20.0	10.0	ND	ND	65
3-1/2482	End Edge	2.0	1.0	2.0	2.0	2.0	<10.0	6.0	ND	ND	47
42/1960	End Edge	20.0	10.0	<1.0	2.0	2.0	<10.0	6.0	ND	ND	62
10/9977C	End Center	2.0	ND	ND	1.0	2.0	<10.0	6.0	ND	ND	
12-1	Mid Length Edge	4.0	4.0	ND	1.0	4.0	20.0	8.0	ND	ND	
	Mid Length Ctr	1.0	ND	ND	1.0	4.0	10.0	ND	ND	ND	
0001/1743	QC Edge Slab	4.0	ND	ND	ND	ND	10.0	ND	ND	ND	< 43
0001/2820	QC Edge Slab	4.0	ND	ND	ND	ND	20.0	ND	ND	ND	54
0018/1787	QC Edge Slab	2.0	ND	ND	6.0	ND	40.0	4.0	ND	ND	< 37
0018/2511	QC Edge Slab	ND	ND	ND	ND	ND	ND	1.0	ND	ND	61
0175/615	QC Edge Slab	ND	ND	ND	ND	ND	10.0	ND	ND	ND	< 32
0175/2520	QC Edge Slab	4.0	4.0	ND	8.0	ND	20.0	ND	ND	ND	< 35
9974	QC Edge Slab	1.0	4.0	ND	2.0	ND	10.0	ND	ND	10.0	14
9987	QC Edge Slab	ND	ND	ND	1.0	ND	10.0	ND	ND	<10.0	21
9990	QC Edge Slab	ND	ND	ND	1.0	ND	10.0	ND	ND	<10.0	93
0060	QC Edge Slab	ND	ND	ND	1.0	ND	10.0	ND	ND	<10.0	93
Sensitivity, Min Content Detectable		1.0	0.5	1.0	1.0	1.0	10.0	1.0	0.5	10.0	

(a) Accuracy -25% to +35% = 1 σ ; for 90% confidence x 1.5.

(b) Not detected.

(c) Ash content determined by Great Lakes Carbon Corporation.

TABLE 3.3
IRON, TITANIUM, VANADIUM, BORON, AND ASH IN H-327 GRAPHITE
(Logs Selected from Various Stages in Fuel Block Production Schedules)

Production Period	Impurity, Max/Min/Mean Content and Standard Deviation (ppm)																			
	Total Ash				Boron				Iron				Titanium				Vanadium			
	$\bar{x}$	σ	Max	Min	$\bar{x}$	σ	Max	Min	$\bar{x}$	σ	Max	Min	$\bar{x}$	σ	Max	Min	$\bar{x}$	σ	Max	Min
First three graphitizing runs, 3/69 (approx 70 logs)	56.1	53.8	408	16.0	0.5	0.48	4.6	0.1	9.92	16.37	80.0	1.0	4.35	7.72	33.4	0.2	1.22	1.54	8.7	0.1
Log 42/2034, from mid-schedule, May 1969	60 ^(a)				0.5				6.0				<1.0				<0.5			
Logs from late stage of graphitization schedule, August 1970, Sept. 1970	199 192				ND ^(b) ND ^(b)				80.0 20.0				8.0 20.0				10.0 10.0			

^(a) Accuracy -25% to +35% 1 σ .

^(b) Not detected, less than 0.5 ppm.

TABLE 3.4
TENSILE TEST DATA FOR LATE PRODUCTION GRADE A GRAPHITE
(Log R1600 42/1536 (F-43) 4-2, Graphitized 8/7/70)

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:	2740	2390	908	1510	1470	1080	697	1380
Maximum	2920	2720	1070	1600	1610	1130	725	1400
Minimum	2440	2390	867	1090	1370	1060	562	368
Mean	2700	2500	948	1400 ^(a)	1483	1090	661	1049
Cumulative Mean	2407	2429	1425	1985	1427	1229	845	1274
Current Std Deviation	256	211.8	368	431	143	143	161.6	358
STRAIN AT FRACTURE, in./in. x 10 ⁻³ :	2.7	2.6	1.45	1.7	3.7	2.5	1.61	3.55
Maximum	2.7	2.8	1.5	2.0	3.82	2.55	1.7	3.8
Minimum	2.25	2.5	1.3	1.65	3.0	2.5	1.6	0.75
Mean	2.55	2.63	1.42	1.78	3.5	2.52	1.63	2.70
Cumulative Mean	2.092	2.287	1.46	1.89	3.093	2.692	1.95	2.827
Current Std Deviation	0.493	0.525	0.193	0.24	0.689	0.385	0.439	1.008
SECANT MODULUS, x 10 ⁶ psi:	1.08	0.91	0.666	0.8	0.435	0.443	0.45	0.389
Maximum	1.08	1.09	0.72	0.915	0.456	0.452	0.45	0.45
Minimum	1.02	0.85	0.626	0.640	0.385	0.415	0.33	0.36
Mean	1.06	0.95	0.67	0.785	0.424	0.437	0.41	0.399
Cumulative Mean	1.212	1.138	1.061	0.93	0.513	0.465	0.45	0.463
Current Std Deviation	0.248	0.263	0.118	0.296	0.168	0.053	0.077	0.094
CHORD MODULUS 250-500 psi (10 ⁶ psi):	1.54	1.06	0.83	1.0	0.625	0.5	0.47	
Maximum	1.82	1.136	0.833	1.09	0.64	0.526	0.47	0.57
Minimum	1.42	1.000	0.72	0.66	0.54	0.471	0.33	0.44
Mean	1.59	1.065	0.79	0.91	0.601	0.349	0.423	0.505
Cumulative Mean	1.682	1.518	1.316	1.367	0.650	0.607	0.54	0.637
Current Std Deviation	0.607	0.603	0.503	0.384	0.208	0.232	0.174	0.256

(a) Vendor's test 1605 psi.

TABLE 3.5
TENSILE TEST DATA FOR LATE PRODUCTION GRADE A GRAPHITE
(Log R1736 42/247 (F-36) 13-1, Graphitized 9/14/70)

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:	2080	1790	1410	1030	1300	1020	1100	1560
Maximum	2140	1900	1410	1160	1300	1120	1100	1650
Minimum	1820	1790	1070	883	1240	832	749	1340
Mean	2013	1827	1296	1024 ^(a)	1280	1007	983	1516
Cumulative Mean	2325	2279	1395	1793	1397	1174	877	1323
Current Std Deviation	286	328	334	554	142	173	173	338
STRAIN AT FRACTURE, in./in. x 10 ⁻³ :	2.17	2.08	1.13	1.3	3.3	2.50	2.55	4.0
Maximum	2.70	2.4	1.27	1.35	3.5	2.86	2.85	4.5
Minimum	1.70	1.87	1.13	1.0	3.0	2.40	1.5	3.2
Mean	2.19	2.12	1.18	1.22	3.26	2.59	2.3	3.9
Cumulative Mean	2.045	2.241	1.395	1.756	3.128	2.652	2.03	3.04
Current Std Deviation	0.460	0.468	0.211	0.358	0.622	0.342	0.50	1.028
SECANT MODULUS, x 10 ⁶ psi:	0.960	0.790	1.11	0.89	0.375	0.390	0.437	0.412
Maximum	1.070	0.860	1.12	1.03	0.433	0.425	0.49	0.418
Minimum	0.306	0.615	0.947	0.66	0.371	0.330	0.386	0.340
Mean	0.778	0.755	1.05	0.86	0.39	0.381	0.404	0.390
Cumulative Mean	1.125	1.042	0.971	0.917	0.489	0.446	0.44	0.449
Current Std Deviation	0.324	0.288	0.201	0.273	0.157	0.062	0.07	0.090
CHORD MODULUS 250-500 psi (10 ⁶ psi):	1.14	1.02	1.39	1.19	0.446	0.50	0.50	0.50
Maximum	1.92	1.33	1.82	1.31	0.500	0.54	0.53	0.5
Minimum	0.925	1.00	1.11	0.806	0.400	0.30	0.50	0.4
Mean	1.33	1.11	1.44	1.10	0.448	0.446	0.510	0.466
Cumulative Mean	1.612	1.417	1.344	1.314	0.610	0.570	0.53	0.603
Current Std Deviation	0.592	0.551	0.463	0.371	0.203	0.219	0.5	0.239

(a) Vendor's test - 1620 psi.

The lowest measured strength in the parallel direction in the mid-length of a log is 867 psi with a mean value at this location for this particular log of 948 psi. The cumulative mean tensile strength of all logs at this position is 1400 psi, and the strength of a fuel block will be greater by approximately 1000 psi at the outer fiber positions in the parallel-to-extrusion direction (i.e., the longitudinal axis of the fuel element).

The lowest measured transverse tensile strength of specimens machined from a PSC fuel block is 368 psi at the end-center position, with a mean value for this location in this particular block of 1049 psi. The transverse direction cumulative mean strength ranges from 843 psi at the center to 1427 at the outer fiber positions.

The relative variability* in tensile strengths is 10% up to 30% at the outer and inner fiber positions. The strength of the inner fibers of a fuel block is 60% of the maximum strength of the block and the variability of the measured values of cumulative mean stress is much greater at this position.

Irradiation Testing

Capsule F-26. Postirradiation examination of capsule F-26 has been completed and the analysis of the results is in progress. Initial results covering the macroscopic examination of the fuel rods along with the macro- and microscopic examinations of the loose particle tests were reported in the previous quarterly report (GA-10444). A summary of the metallographic examination and the tentative conclusions drawn from this experiment (dependent upon final temperature analysis results) are given below.

As was reported in GA-10444, five of the eight hot-injected fuel rods survived the F-26 irradiation. Four of these rods were examined metallographically (F-26-4A, -4C, -4E, and 5E); rod F-26-4B was designated for fission-

*Pearson's coefficient of variation (Current Std Deviation/Cumulative Mean).

product release studies and was not examined metallographically. The microscopic investigation found these four rods to be in relatively good condition (see Figs. 3.5 through 3.8). Little difference could be detected between the appearance of the matrix microstructure of these and identical rods that were irradiated to lower fast fluences in capsule F-25 (see earlier quarterly report GA-10313). The peak exposure for the capsule F-25 fuel rods was 4.8×10^{21} n/cm² compared to 7.0×10^{21} n/cm² for capsule F-26. These results indicate that both the natural-flake and isotropic matrices have not deteriorated significantly with the increased irradiation exposure and have good stability to these high fast-neutron exposures.

No deleterious effects on the particle coatings (with the one possible exception noted below) were noted that could be attributed to particle-matrix or particle-particle interactions, and it appears that these matrix and particle combinations are physically compatible under the severe irradiation conditions experienced by this capsule.

Calculations of capsule F-26 fuel rod dimensional changes based on particle shrinkage data (see earlier quarterly report GA-10313) found the calculated values to be in excellent agreement with measured dimensions. An example is shown in Fig. 3.9, which compares the calculated dimensional change profile with measured values for rods 4B irradiated in capsules F-25 and F-26. These data show that dimensional changes of fuel rods containing TRISO particles can be predicted to fast-neutron fluences of 7.0×10^{21} n/cm² and that the particles are still controlling the dimensions at these high exposures.

An increase from the F-25 particle coating failures was observed in some of the F-26 rods, particularly for the fertile particles. This result appears to be consistent with results from thimble tests of loose particles, which also showed slight to moderate increases in fertile particle coating failure. A few of the fertile particle failures in rod F-26-5E looked as if they might have resulted from particle-particle interactions (see Fig. 3.10). However, because the number of failures was small and some coating failure would be expected due to irradiation effects alone,

M400-125

(a)

(~260X)

EDGE (K85690)

(b)

CENTER

Fig. 3.5. Photomicrographs of fuel rod 4A irradiated in capsule F-26 to $7.0 \times 10^{21} \text{ n/cm}^2$ at 1250°C (hot-injected, pitch binder, natural-flake graphite filler, 2000°C preirradiation heat-treatment, TRISO-propane coated particles): (a) typical matrix graphite; (b) composite radial cross section

M400-9

(a)

(~260X)

EDGE

(K85826)

(b)

CENTER

Fig. 3.6. Photomicrographs of fuel rod 4C irradiated in capsule F-26 to $6.8 \times 10^{21} \text{ n/cm}^2$ at 1250°C (hot-injected, pitch binder, natural-flake graphite filler, 1000°C preirradiation heat-treatment, TRISO-propane coated particles): (a) typical matrix graphite; (b) composite radial cross section

M400-175

(a)

(~260X)

EDGE

(K85689)

(b)

CENTER

Fig. 3.7. Photomicrographs of fuel rod 4E irradiated in capsule F-26 to $6.6 \times 10^{21} \text{ n/cm}^2$ at 1250°C (hot-injected pitch binder, isotropic graphite filler, 2000°C preirradiation heat-treatment, TRISO-propane coated particles): (a) typical matrix graphite; (b) composite radial cross section

M400-302

(a)

(~260X)

EDGE

(K86143)

(b)

CENTER

Fig. 3.8. Photomicrographs of fuel rod 5E irradiated in capsule F-26 to $5.1 \times 10^{21} \text{ n/cm}^2$ at 1250°C (hot-injected pitch binder, isotropic graphite filler, 1800°C preirradiation heat-treatment, TRISO mixed-gas coated particles): (a) typical matrix graphite; (b) composite radial cross section

Fig. 3.9. Comparison of calculated and measured diametral changes for 4B rods from capsules F-25 and F-26

M800-300

(a)

(70X)

M400-297

(b)

(70X)

Fig. 3.10. Photomicrographs showing failed fertile particles from fuel rod F-26-5E

it was not possible to confirm that particle-particle interactive stresses caused the failures.

Reasons for the poor performance of the three rods which failed (F-26-1A, -1E, and -5A) were fairly evident from both the macroscopic (see earlier quarterly report GA-10444) and metallographic examinations. Fuel rod F-26-1A was poorly injected with matrix (see Fig. 3.11) and cracked in areas where there was little or no matrix; portions that were better injected remained bonded.

The F-26-1E rod was evidently attacked by gaseous impurities (see Fig. 3.12) which most probably entered the cell through a rupture in the lead tube which was damaged in the reactor during operation. The rod was badly eroded at one end and along its surface. A considerable percentage of the particles debonded. Most of the debonded particles which were examined metallographically showed signs of external attack; however, the extent of attack varied widely from particle to particle with some particles having only slightly pitted coatings while others had severely eroded and failed coatings. A few particles even had oxidized kernels [see Fig. 3.13, (a) and (b)]. It was interesting to note that the kernels of a few fissile particles that had failed coatings remained protected, apparently by the buffer [see Fig. 3.13 (c)]. (It is possible that the buffers contained a high concentration of fission products that may have afforded some oxidation protection to the kernel.)

Metallographic examination of fuel rod specimen F-26-5A confirmed macroscopic examination results that showed the fertile particles had a very high percent of failed coatings (see Fig. 3.14). This rod contained fertile batch 4000-194, which has failed badly in previous capsule tests as a result of the relatively highly oriented outer pyrocarbon coating. The rod was intact when removed from the capsule, but it debonded in storage. The metallographic results showed a number of the fertile kernels hydrolyzed and expanded, apparently when exposed to the moist hot-cell atmosphere, and this is the probable reason the rod eventually debonded in storage.

M400-23

(a)

(~260X)

EDGE

(85538)

(b)

CENTER

Fig. 3.11. Photomicrographs of fuel rod 1A irradiated in capsule F-26 to $4.7 \times 10^{21} \text{ n/cm}^2$ at 1250°C (hot-injected pitch binder, natural-flake graphite filler, TRISO mixed-gas coated particles): (a) typical matrix graphite; (b) composite of partial radial cross section. Rod was inadequately injected with matrix and partially debonded under irradiation

M400-69

(a)

(~260X)

EDGE

(K85635)

(b)

CENTER

Fig. 3.12. Photomicrographs of fuel rod 1E irradiated in capsule F-26 to $5.8 \times 10^{21} \text{ n/cm}^2$ at 1250°C (hot-injected pitch binder, isotropic graphite filler, 1800°C preirradiation heat-treatment, TRISO-propane coated particles): (a) typical matrix graphite; (b) partial radial cross section, edge has been eroded away. The poor condition of the rod resulted from impurity gas attack

M400-48

(a)

(~80X) M400-338

(b)

(~130X)

M400-360

(c)

(~175X)

Fig. 3.13. Photomicrographs of fuel particles from the debonded portion of fuel rod F-26-1E. Particles shown in (a) and (b) illustrate varying degrees of impurity gas attack observed: (a) shows slightly pitted OPyC coating; (b) shows particle that has suffered severe erosion of all coatings with kernel apparently completely converted to oxide; (c) shows badly eroded coatings, but fuel kernel appears to have been protected from oxidation by buffer layer

M400-29

(a)

(60X)

M400-41

(b)

(300X)

Fig. 3.14. Photomicrograph of fuel particles from fuel rod 5A irradiated in capsule F-26 to $6 \times 10^{21} \text{ n/cm}^2$ at 1250°C (hot-injected, pitch binder, natural-flake graphite filler, 1800°C preirradiation heat-treatment, TRISO-propylene coated particles). Fuel rod contained a bad fertile particle batch that had high failure; rod fell apart in storage: (a) typical appearance of coated particles after irradiation; (b) appearance of matrix graphite

Microscopic examination of the Si-SiC diffusion-bonded fuel rods yielded results similar to those obtained in previous irradiations.

The tentative conclusions drawn from the capsule F-26 experiment are summarized below, and are in general agreement with the conclusions from the previous F-capsule irradiation tests. These conclusions are:

1. Prototype hot-injected fuel rods have demonstrated satisfactory performance to 7.0×10^{21} n/cm² or to approximately 90% of full Fort St. Vrain reactor exposure.
2. Hot-injected fuel rods containing TRISO particles have good dimensional stability, and the dimensional changes are controlled by the particles.
3. Matrices using a pitch binder and both natural-flake and isotropic graphite fillers have shown good irradiation stability, and currently there is no evidence that shows one filler is superior to the other.
4. High coating failure and poor matrix injection are undesirable and have been the principal causes of rod failure. High coating failures in fuel rods mainly have been attributed to coating parameters rather than to effects resulting from the particles being incorporated into the rods.
5. Reference TRISO fissile particles have shown excellent irradiation stability in rod and thimble tests to full reactor exposure, and although reference TRISO-fertile particles have consistently shown slight failures, they have been within acceptable limits for maintaining fuel rod integrity.

Capsule F-30. Capsule F-30 will be the final proof test of Fort St. Vrain reactor fuel to full exposure. The capsule will contain four fuel rod cells that will operate to (1) 8×10^{21} n/cm² at 1050°C, (2) to 4×10^{21}

n/cm² at 1250°C, and (3) to 5×10^{21} n/cm² at 1050°C. These three fluence-temperature combinations will test six-year production fuel to conditions representative of (1) the maximum fluence in a six-year core at its associated maximum temperature, (2) the maximum temperature in a six-year core at its associated maximum fast fluence, and (3) the maximum fluence in a four-year core at its associated maximum temperature. A fifth cell will contain coated particle thimble samples tested to 8×10^{21} n/cm² at about 1050°C.

Eleven rod samples will be tested in the capsule. These include five 2-in.-long rods that will be identical to real production rods and six 0.7-in.-long rods which will contain thermocouples. The capsule is being designed to ensure that most rods will operate under heat generation rates and temperature gradients representative of reactor conditions. As such, inert particles will have to be used in some rods.

The purge gas stream from each cell will be monitored to determine in-pile fission-gas release, and the preirradiation release will also be measured on all rods used in the capsule. The activity levels in lightly irradiated fuel rods appear sufficiently low (approximately 75 mr at 2 in. after 11 days decay) to allow release measurements to be made on the actual capsule rods.

The schedule for Capsule F-30 will be determined by the availability of representative production particles and fuel rods. At present, the June GETR cycle (129) appears to be the earliest that F-30 will begin irradiation.

ORNL Capsule HRB-2. Capsule HRB-2, which contains Fort St. Vrain reactor production coated particles and fuel rods, is beginning its third cycle in the HFIR. It continues to operate very well and to exhibit low release (1 to 5×10^{-5} for Xe and Kr isotopes) after about 2.0×10^{21} n/cm² at 1150°C. The capsule is designed to be irradiated to 8×10^{21} n/cm² at 1150°C in eight HFIR cycles. (Duplicate samples will receive about 4×10^{21} n/cm² at 750°C in a lower flux region.)

Capsule Temperatures. The first set of CAPTEM runs covering fuel bed temperatures in capsules F-25 through F-29 has been completed. A thorough review of the input data used in these calculations revealed uncertainties in primary gas mixture compositions and flows, and work is now in progress to determine the correct values. Refinements were made in some materials thermal and irradiation property input data, including fuel rod thermal conductivity based on macroporosity. Several code modifications for additional data printout and format changes are also being made. As soon as these corrections and refinements are complete, a second set of CAPTEM runs will be made for the above capsules, which should result in the best temperature data available to date on these experiments.

An analysis of uncertainties in calculated temperatures of fuel rod irradiation samples using the CAPTEM code showed that a 95% probability exists that with the presently estimated uncertainties the error in the calculated fuel rod centerline temperature is $<160^{\circ}\text{C}$. The only way to cut down this error is to improve knowledge of fuel conductivities, gas conductivities (gap widths), and thermal fluxes.

Graphite Irradiations. A number of graphite piggyback samples of varying length have been included in the P13M irradiation capsule, which is a scheduled full-exposure test of fuel samples. The graphite samples of interest are a number of 0.105- and 0.080-in.-diameter H-327 graphite samples manufactured in the latter half of the PSC manufacturing schedule using No. 2 pitch. This pitch has a narrower mid-range content of quinoline and benzene insolubles than that of the original No. 1 pitch used for the production of the first half of the core graphite. No differences in dimensional change behavior of the new materials is expected.

Samples of H-327 graphite crucibles used in the F-27 and F-29 fuel irradiation capsules have been sectioned and measured for thermal conductivity changes in the range of room temperature to 800°C . Values were obtained by the xenon-flash method. The maximum irradiation temperature of the available samples was 800°C , and the thermal conductivities in the transverse direction (across the fuel block web) at the proposed PSC maximum and median fluence

levels are shown in Figs. 3.15 and 3.16. At irradiation temperatures between 390° and 800°C, there are no marked differences in the reduction of thermal conductivity at around median fluence levels, the maximum reduction being 50% at the expected lowest service temperature of 450°C (see Fig. 3.15). However, the changes in thermal conductivity appear to be a function of the inter-related conditions of fast-neutron dose ($E > 0.18$ MeV) and temperature, and are much less at the higher irradiation temperature for a given fluence, at least at maximum reactor doses (see Fig. 3.16). The maximum reduction in thermal conductivity is 22% at the minimum design temperature for graphites irradiated at mean reactor temperatures of around 800°C. At service temperatures of 800°C, the measured changes in thermal conductivity of graphite irradiated to the full dose at this temperature is less than 8%.

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Fig. 3.15. Thermal conductivity in the transverse direction of unirradiated and irradiated H-327 graphite at design maximum fast fluence

Fig. 3.16. Thermal conductivity in the transverse direction of unirradiated and irradiated H-327 graphite at approximate design median fast fluences

TASK IV

FUEL ELEMENT PROOF TESTING

The objective of this task is to demonstrate suitable performance of a test fuel element or elements, 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.

Proof test element No. 2 continues to operate well in Peach Bottom Core 2. The Chromel-Alumel temperature reading between November 1970 and February 1971 increased slightly from 1690°F to 1700°F due to an increase in power level of approximately 1%.

The element has generated 433 MW-hr during the quarter, for a lifetime total of 1692 MW-hr.

The design of proof test element No. 3 is complete. The design is of the fuel test element type employing eight-hole telephone-dial fuel bodies, as described in the last quarterly report (GA-10444). The radial gap between the center body and the sleeve was increased from 0.045 to 0.065 in. as a result of further thermal analysis. Maximum fuel temperature should reach 2100°F with this gap.

Fabrication of the test element has started. The fuel particles have been made and are being qualified, and fuel rod fabrication will begin shortly. Thermocouples are on order. Graphite fuel bodies are being machined. All other components are available for final assembly.

PART II

PRIVATELY SPONSORED RESEARCH AND DEVELOPMENT PROGRAM

(Not the Subject of AEC Reimbursement)

TASK 1
FUEL TRANSFER MACHINE

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

Disassembly, refurbishment, and shipment of the fuel transfer machine to the site has been completed. As a result, no further reporting on this task is expected.

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 include helium-side heat transfer and tube vibration.

TUBE PLUGGING MACHINE

Assembly of module No. 1 (the machine preparation head) was finished, thus completing the three modules which comprise the tube plugging tool assembly.

Testing will begin when the seal welding equipment and controls are returned from the Fort St. Vrain site.

INSTRUMENTATION BRAZING

Brazing of the thermocouples and strain gauges into the instrumentation plugs was completed, Module 8 being the final unit to be brazed. Several thermocouples have accidentally been broken during installation of the brazed ports, which have required repairs to be carried out. One in particular on Module 8 required some development work to make a satisfactory fix.

INCOLOY 800 MATERIALS

All required heats of Incoloy 800 materials were examined to confirm heat treatment conditions. All were found to be satisfactory.

PRIMARY CLOSURE O-RING SEAL

Problems arose with silver plating flaking off the O-ring seal and with unsatisfactory weld joints in the ring which plugged the O-ring bore. Examination showed them all to be unsatisfactory and new parts were ordered.

MODULE AIR-FLOW TEST

The heat-transfer performance tests of the main steam system (economizer-evaporator-superheater and superheater II) were completed at air-flow rates corresponding to 100%, 62%, and 25% of design helium Reynolds number. The Nusselt numbers are compared to those obtained with the Grimson correlation in Table IV.1.

TABLE IV.1
HEAT-TRANSFER PERFORMANCE DATA

	25% Flow	62% Flow	100% Flow	116% Flow
EES:				
Nu experimental	93.3	147.8	191.8	---
Nu Grimson	89.4	137.5	180.9	---
SH-2:				
Nu experimental	94.5	161.6	214.6	238.4
Nu Grimson	81.6	132.8	180.3	192.6

The results of the mixing tests for the main steam bundles were incorrect because of incorrect thermocouple grounding. These tests will be re-run as soon as reheater testing is completed.

Results of the reheater testing to date indicates performance to be lower than predicted at the lower flow rates of 62% and 25%.

Velocity probe data were obtained at the reheater inlet and outlet, and a radial flow distribution problem was observed. The air-flow velocity

at the bundle inlet was as high as twice the average velocity at the center, and a flow reversal was evident at the outer shroud (see Fig. IV.1). A perforated plate structure was designed and installed based upon water table studies to try to even out the air flow at the bundle inlet. Retesting indicated a worse flow distribution, however, with peaks up to 2.4 times the average velocity. The plate has been removed, and testing will continue until a new flow-distribution device has been developed from water table studies.

The reheater performance problem has partially been attributed to poor tube-to-tube water flow distribution due to air blockage of certain reheater tubes. A venting arrangement was installed in the reheat header to assure the removal of air from the reheater. Testing with the vented system showed flow through all reheater tubes as demonstrated by thermocouple measurements.

WATER TABLE STUDIES

A water table was built to permit studies of flow distribution to the reheater inlet section of the steam generator module (see Fig. IV.2). The water table was used to test various configurations of inlet screens, baffles, and vanes to predict their effect in redistributing the velocity profile shown in Fig. IV.1.

For ease of installation, a basket type of screen was selected for testing in the air-flow test module as a first attempt. Testing revealed, however, that the relatively small wakes indicated on the water table resulted in an unacceptable flow distribution in the air-flow test module. Figures IV.3 through IV.5 show the flow distribution entering the reheater section as is, with a simulated tube bundle and with the basket installed upstream from the reheater. Further water table flow studies are being executed with screens of various configurations and void factors, as well as various vane systems. A new redistributing device will be selected for tests in the air-flow module based on water table test results.

Fig. IV.1. Local/average air velocity

Fig. IV.2. View of water table used for flow distribution studies

G87301

Fig. IV.3. Flow distribution entering reheater section with as-is configuration

HT87299

Fig. IV.4. Flow distribution entering reheater section with simulated tube bundle

HT87298

Fig. IV.5. Flow distribution entering reheater section with basket installed upstream from the reheater

The tests were conducted in two phases. The first consisted of slowly throttling the air flow from maximum down to zero while observing the strain gage signals, four at a time, on an oscilloscope. If a resonant condition had developed during this run, the air flow would have been held constant and the signals recorded, but none was observed. The second phase consisted of reducing air flow in 5% increments from maximum down to 20%, and obtaining a complete set of recordings at each air-flow rate. A complete set of recordings was also obtained at zero air flow with the blower running and shut down, to obtain the influence of electrical and acoustical noise on the strain gage signals.

The test results indicated very low strain levels. When converted from strain to stress and corrected for fluid density ratio and gage location, the maximum vibration stress is 342 psi. Table IV.3 shows a comparison of predicted to experimental stresses and frequencies for a long reheater lead tube, a long crossover tube, and a long downcomer tube. These data represent the maximum vibration stresses measured for each class of lead tubes and is the maximum along the tube length.

The measured vibration stresses on helical coils and on shrouds and insulation covers was, in all cases, below the noise level (approximately 100 psi) of the instrumentation. In no case did the experimental stress levels exceed the predicted values. The predicted stress levels have been incorporated in the final PSC stress report, and the experimental stresses are therefore acceptable.

The vibration stresses measured on the helical tubes are generally lower (approximately 50%) than those measured during the GAVEL test. The GAVEL test was an air-flow test of a full-scale section of the economizer-evaporator-superheater helical bundle without lead tubes. The test bundle was fabricated and the test conducted with only three tube support plates. This was before the design was changed to three full plates and three "floating" plates. The tube spans were therefore longer in the GAVEL bundle, which explains the higher vibration stresses.

TABLE IV.3
LONG LEAD TUBE STRESS AND FREQUENCY SUMMARY

	Downcomer Tube No. 41			Crossover No. 8			RH Lead Tube No. 10		
	Mode 1	Mode 2	Mode 3	Mode 1	Mode 2	Mode 3	Mode 1	Mode 2	Mode 3
Frequency, cps:									
Analysis	12	16	--	8	15	22	29	51	59
Test	13	21	--	--	20	24	31	--	64
Maximum stress, psi:									
Analysis	1700	1300	--	210	299	379	166	254	245
Test	90	150	--	--	149	342	255	--	183
Calculated stress range									
Including vibration, psi ^(a)		24,850			37,100			26,264	
Maximum allowable									
Total stress, psi ^(a)		37,500			39,000			39,000	

(a) From Final Stress Report.

The measured maximum vibration stresses for the downcomer tubes are only 5% to 10% of the predicted values. This result is explained by the conservative assumption of resonance in the analysis, which may not have occurred during testing.

During the tests, vibration of two crossover tubes was observed through the window between the economizer-evaporator-superheater and superheater II tube bundles. These tubes did not have strain gages. Accelerometers were attached to determine tube deflection in three directions. An optical transducer was also used as a check on the accelerometers. Amplitudes of up to 0.009 in. half amplitude were measured. This deflection converts to a corrected stress level of approximately 320 psi, which is roughly equivalent to the stress levels measured on other crossover tubes that had strain gages.

From the vibration test results it is concluded that

1. Vibratory stresses are acceptable and will not cause tube failures.
2. Analytical prediction methods are adequate and give conservative predictions for vibratory stresses.

Appendix
PROJECT REPORTS PUBLISHED DURING THE QUARTER

There were no topical reports published during this period.