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AEC RESEARCH AND
DEVELOPMENT REPORT

GA-9372

HTGR BASE PROGRAM
QUARTERLY PROGRESS REPORT
FOR THE PERIOD ENDING
May 30, 1969

Prepared under
Contract AT(04-3)-167
Project Agreement No. 17
for the
San Francisco Operations Office
U.S. Atomic Energy Commission

June 27, 1969

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Gulf General Atomic Incorporated

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INTRODUCTION

This report covers the work performed by Gulf General Atomic Incorporated under U.S. Atomic Energy Commission Contract AT(04-3)-167, Project Agreement No. 17. This Project Agreement calls for preliminary design and related exploratory development of a 1000-Mw(e), gas-cooled, nuclear power reactor incorporating advanced technology, and a 150-300 Mw(e) prototype. This program is based on the concept of the High-temperature Gas-cooled Reactor (HTGR) system developed by Gulf General Atomic.

The related exploratory development effort is aimed at a 1000-Mw(e) gas-cooled reactor plant that could be placed in operation in the 1970's, with operation of the prototype at an earlier date. The plant would demonstrate such new design features as:

1. Breeding or near breeding of fuel in a high-temperature, gas-cooled, thermal reactor using a Th-U²³³ fuel cycle.
2. Advanced reactor fuel elements, which may include new and simpler configurations and new methods of processing and fabricating to extend fuel lifetime and reduce fuel costs.
3. Alternative plant arrangements to reduce plant capital costs, including such possibilities as alternative materials (e.g., concrete pressure vessels) and the assembly of the reactor and steam-generating equipment in a single containment.

Physics, metallurgical, chemical, electronic, and analytical design and developmental engineering programs support the primary reference design objective.

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Task I

1000-MW(E) ENGINEERING DEVELOPMENT STUDIES

ECONOMIC ASSESSMENTS

The general and specific ground rules, briefly outlined in the previous progress report (GA-9227), were reviewed and expanded during the current reporting period. The detailed ground rules to be used in the Base Program are described in this report.

The general economic assessment provisions are:

1. The economic assessments will be based on the power production cost of the alternatives considered during the program. The power costs will be obtained from the combined estimates of capital, operating, and fuel cycle costs. The "equivalent capital cost" will not be used in order to prevent confusion with the capital cost estimate values defined in Ref. 1, TID-7025, Guide to Nuclear Power Cost Evaluation.
2. Cost estimates will be based on designs that are considered feasible. Although engineering development may be required to make the designs practicable, a technological breakthrough is not required.
3. Private ownership of all power plant facilities, fissile materials, and fuel fabrication and reprocessing plants will be assumed.
4. The total electrical capacity of the reactor plant will be 1000 Mw(e); however, the fuel fabrication and processing facilities will be capable of servicing several reactors of the same type. A reference capacity of 15,000 Mw(e) will be used for these facilities.
5. The power generation costs will be based on an annual plant operating factor of 80% of the net power rating. The fuel management program will be such that the total annual shutdown time, including scheduled outages for fuel reloading or reshuffling and for scheduled inspection and maintenance, will not exceed 870 hr (i.e., 90% plant availability). A 30-year economic life of the plant will be assumed.
6. The general ground rules defined in Ref. 1 will be adopted, where applicable, to the HTGR; however, the new ground rules will be substituted where necessary to complete the HTGR assessments or where factual data or recent experience is available. An example of this is the recent interest rates and tax data applied to the determination of annual fixed charges.

Reactor Plant Capital Cost

Direct construction costs will be estimated for the entire power plant, with the emphasis on those systems and components affected by the design alternatives under consideration. In estimating the construction costs, it

will be assumed that the equipment and the system have been fully developed and that the plant is one of a number of the same type to be built. Construction costs will include all equipment up to the low-voltage side of the step-up transformer. Gateway structures, switchyard, step-up transformer and transmission lines will not be included in the estimate. Capital cost breakdowns will be prepared in accordance with the Gulf General Atomic Incorporated system of accounts and will be converted, when required for reporting purposes, to the AEC system of accounts.

The design and reheat cycle alternatives under consideration during the program will result in some variation in power plant electrical output for a given reactor thermal power because of variations in over-all plant efficiency. The economic assessments will use a "capability adjustment" to estimate the change in reactor plant cost required to return the electrical output to the Base Case value of 1000 Mw(e), rather than to design a reactor core for each alternative. An incremental capability value of \$60/kw will be used to establish the estimated direct construction cost (ΔC) increase or decrease, as a function of the difference in plant efficiency ($\Delta \eta$) from the Base Case plant efficiency (η), as follows: $\Delta C = -\$60,000,000 \Delta \eta / \eta$.

Indirect construction costs will consist of the general and administrative, miscellaneous construction, engineering design and inspection, contingencies, and interest during construction, applied as a cumulative percentage of the direct costs, and startup costs applied as a percentage of the annual operating and maintenance costs. The percentages to be used are listed below.

General and administrative	6.1%
Miscellaneous construction	1.0%
Engineering design and inspection:	
Architectural and engineering services	5.1%
Nuclear engineering	2.1%
Startup costs	35% of O & M
Contingency	10%
Interest during construction	17.3%

The total depreciable capital cost will consist of the direct and indirect construction costs described above, including interest during construction. The cost of the initial helium charge will be considered to be a depreciable capital cost, even though it will have some recoverable value at the end of plant life.

The total nondepreciable capital cost will consist of the cost of land and land rights plus the value of working capital. Land and land rights will be assumed to cost \$360,000, as estimated in Ref. 2. The working capital will be estimated as 2.7% of the annual fuel and operating and maintenance costs, plus the average value of the nuclear fuel inventory, plus other materials and supplies (assumed to be 25% of the annual cost of maintenance materials and operating supplies).

Operation and Maintenance

The current estimate of operation and maintenance costs for the power plant are based on the organization given in Fig. 1.1. The cost breakdown is shown in Table 1.1, and is the estimated cost to be used during the course of the program.

Table 1.1
OPERATION AND MAINTENANCE COSTS

Annual payroll at 56 people maximum	\$523,560	
Fringe benefits at 33-1/3%	<u>174,520</u>	
Total annual labor costs		\$698,080
Operating supplies:		
Repair and maintenance materials	271,100	
Utility water	<u>40,000</u>	
Total annual operating supplies costs		<u>311,100</u>
Annual direct operation and maintenance costs		1,009,180
General and administrative overhead at 10%		<u>100,920</u>
Total annual operation and maintenance costs		<u>\$1,110,100</u>

Fuel Cycle Cost

The reference fuel cycle will recycle the U^{233} throughout the reactor lifetime. Any additional fissile material required (including the entire initial loading) is supplied in the form of 93% enriched uranium. The fuel cycle cost will be established in terms of mills/kw-hr using the Task I Base Case plant efficiency of 39.4%. The cost estimate will be based on present worth accounting with the averages taken over a 30-year period and including (1) burnup cost, (2) fabrication cost (including fuel preparation cost and the cost of the replaceable reflector), (3) spent fuel reprocessing cost (including ultimate waste disposal), (4) shipping cost, (5) working capital charges on fissile and fertile material inventories, and (6) working capital charges on fabrication, spent fuel reprocessing, and shipping. The factors involved in developing the fuel cycle costs are described below.

Financing Conventions. Ownership of fissile and fertile materials during fabrication and processing, as well as when on the reactor site, is considered to be vested in the reactor plant. It will be assumed that the fuel is received at the reactor site 45 days before loading. The cooling time before shipment of irradiated fuel is to be 120 days. Working capital charges on fissile and fertile inventories will be computed using a reference value of 10% per year to simplify comparison with other reactors.

Working capital charges on the fabrication, reprocessing, and shipping will be computed in the same way as the working capital on fissile material, again using a 10% rate. A 6% discount factor will be used for computing present worth values.

Fig. 1.1--Representative staff for 1000-Mw(e) HTGR power plant

Value of Materials. The values of the following items will vary with time, depending on such factors as power demand, availability of resources, and types of power plant built. Reference values are given below for current or near-term use, and an anticipated range of values for the longer-term use.

1. Separative work cost. Reference value of \$26/kg and a tails assay of 0.2%; the anticipated range is \$15/kg to \$30/kg with tails assay held constant.
2. Conversion of U_3O_8 . Reference value of \$2.70/kg of uranium; anticipated range is \$1.00/kg to \$2.70/kg.
3. Natural uranium as U_3O_8 . Reference value of \$8.00/lb U_3O_8 ; anticipated range is \$5.00 to \$50.00/lb.
4. Unirradiated thorium as ThO_2 . Reference value of \$5.00/lb ThO_2 ; anticipated range is \$5.00 to \$30.00/lb.
5. Uranium value corresponding to its enrichment. Based on total uranium present, with no penalty for U^{236} content but standard penalty for U^{232} content.
6. U^{233} . 14/12 that of U^{235} in 93% enriched uranium.
7. Plutonium. Reference value from 0.5 to 1.5 times the value of U^{235} in 93% enriched uranium.

Fuel Fabrication and Preparation. The fuel fabrication plant is designed for fabricating a single type of fuel element and capable of serving an electrical capacity of 15,000 Mw(e) of the HTGR reactor type. The plant will be available for operation 270 days per year.

The location will be assumed to be at the same site as the chemical processing plant to permit common use of such utility facilities as electrical power substations, water supplies, steam heating systems and natural gas lines, access roads, and waste treatment and disposal facilities.

The fixed charge rate on depreciating capital will be taken at 22% per year. The unit fuel fabricating cost is assumed to remain the same throughout the 30-year plant life. Holdup time will be established. Losses of plutonium, uranium, and thorium are assumed to be 0.2% per pass.

Fuel Reprocessing. The reprocessing plant is assumed to be designed for reprocessing a single type of fuel element and capable of serving an electrical capacity of 15,000 Mw(e) of the HTGR reactor type. The plant will be available for operation 270 days per year.

A value of 22% per year will be used for the fixed-charge rate on a capital investment, including depreciation, cost of money, taxes, etc. Operating costs will include labor, materials, and all other expenses not included in capital charges, except as noted below.

The fuel inventory in processing will be estimated, but the inventory charges (interest) will not be included in the reported processing cost (i.e., will be reported separately). Losses of fissile and fertile materials will be assumed to be 1%, but the value of the material lost will not be included in the reported processing cost.

Shipping. It will be assumed that fabrication and reprocessing are performed at a site 1000 miles from the reactor. An estimate of average shipping distances for later years when more reprocessing plants are available will be made. Casks will be assumed to cost \$1.00/lb of cask weight and weigh 110 tonnes fully loaded. A cask handling fee of \$500 per round trip and a cask life of 30 years will be used.

The freight rate assumptions will be as follows: loaded cask, \$0.0193/lb; empty cask, \$0.0181/lb; insurance against property loss, 0.05% of the value of the fabricated fuel elements (including the fuel) and the cask for each shipment. (The cost of liability insurance will be included in the charges against the reactor, processing, and fabrication plants.)

Power Generation Costs

The power generation costs for each alternative, which will be compiled from the cost estimates prepared by the methods described above, will consist of the fixed charges associated with depreciating and nondepreciating capital costs and annual nuclear liability insurance, and the operating costs associated with the operating and maintenance costs and the fuel costs.

The capital charge rates will be based on the values derived from the depreciating and nondepreciating capital costs given in Table 1.2.

The cost of annual nuclear liability insurance is based on the Gulf General Atomic best estimate for a 1000-Mw(e) power plant, which is \$256,000 for liability insurance and \$76,000 for federal indemnity for a total of \$332,000.

The annual operating cost figure was presented in Table 1.1.

The fuel cycle costs derived in terms of mills/kw-hr for the Base Case plant efficiency of 39.4% must be adjusted for the calculated plant efficiency of the design or reheat cycle alternative under consideration. The fuel cycle costs will be computed in detail for thermal efficiencies of 37.4%, 38.4%, 39.4%, 40.4%, and 41.4%. Fuel cycle costs at intermediate efficiencies will be computed by interpolation. A value of 1.26 mills/kw-hr at 39.4% efficiency will be used for preliminary studies until detailed evaluations of fuel cycle costs are completed.

Table 1.3 defines the constant values to be used in Task I economic assessments, based on the justifications described above. The organization of the table describes the arrangement of costs used to establish the power generation costs of each alternative in accordance with the guidelines presented in Ref. 1.

Research and Development Costs

Research and development costs will be estimated, where necessary, for each design alternative. The cost will be estimated for the period after the establishment of the reference design (fiscal year 1970) through the year in which the first 1000 Mw(e) is scheduled for startup.

Table 1.2
CAPITAL CHARGE RATES

	Investor-owned Public Utility	
	Depreciating (%)	Nondepreciating (%)
Minimum return required (cost of money)	7.00 ^a	7.00
Depreciation	1.06 ^b	--
Interim replacements	0.35 ^c	--
Property insurance	0.28 ^d	0.28
Taxes:		
Federal income	2.20	
Federal miscellaneous	0.10	
State and local	<u>2.45</u>	
Total taxes	<u>4.75^e</u>	<u>4.75</u>
Total fixed charge rate	13.44	12.03

^aCost of money reflects high cost of debt financing in recent years. A combination of 6.4% bond interest, 8% earnings on equity capital (preferred and common stock) and a debt-capital ratio of 60% would be consistent with an over-all financing cost of 7%.

^bThe derivation of depreciation percentage applies sinking fund depreciation plus the cost-of-money rate to the total investment to determine the annual cost of capital recovery.

^cContinuing studies indicate that an average allowance of about 0.35% of the total investment is required for the 30-year life span of a nuclear power plant.

^dThis value is the Gulf General Atomic best estimate of property insurance through the Nuclear Energy Property Insurance Association.

^eTax estimates are based on weighted averages of tax percentages paid by 128 Class A and B utility companies during the last three to five years.

Table 1.3
POWER GENERATION COSTS

	Capital Cost (\$)	Rate (%)	Annual Cost (\$)	Unit Cost (mills/ kw-hr)
Fixed Charges				
Depreciating capital:				
Total capital cost, less land	(a)	13.44	(a)	(a)
Nondepreciating capital:				
Land and land rights	360,000	12.03	43,300	0.006
Working capital	(a)	12.03	(a)	(a)
Liability insurance ^b			<u>332,000</u>	<u>0.047</u>
Subtotal, fixed charges			---	---
Operating Costs				
Operating and maintenance cost			1,110,100	0.158
Fuel cost			<u>(a)</u>	<u>(a)</u>
Subtotal, operating costs			(a)	(a)
Total Power Generation Costs				
Total Cost			(a)	(a)

^aTo be determined for each alternative.

^bThe annual nuclear liability insurance is considered to be an operating expense in most accounting methods; however, it will be shown as a fixed charge in order to comply with Supplement 1 of TID-7025 (Ref. 1). The economic analysis of this study is not affected by the selection of either accounting method.

Cost Estimating

A computer program has been written and debugged to handle the compilation and control of all construction cost estimates associated with the various components of the reactor power plant. The program is able to present the breakdown of all cost estimates in accordance with either the AEC or the Gulf General Atomic system of accounts, or both. With the incorporation of proper labels, the program should be able to monitor and compare, item by item, cost differentials between different design and cycle alternatives to be considered, or changes in cost estimates associated with a given design.

Parametric studies were performed on the variation of steam generator cost with turbine throttle steam conditions in support of the high-pressure steam turbine circulator drive assessments and the steam cycle options. The results of the studies were factored into system analysis work and will be reported as part of the cycle evaluation studies.

A draft of the cost comparison section of a topical report on refueling methods has been prepared (see section on refueling systems). Preliminary fuel cycle cost data was developed for the off-line and on-line refueling concepts and a preliminary estimate was made of the costs of the equipment variations associated with the three refueling concepts. A preliminary estimate of the cost of replacement power was prepared on the basis of a fossil fuel cost differential of 1.75 mills/kw-hr higher than nuclear fuel costs.

Cost estimates of piping layouts for the Base Case and for the electric-motor-circulator-drive concepts were completed and the data factored into the over-all compilation of costs for the Base Case and the delta costs for the electric-motor-drive concept. Work was initiated on comparative costs of several penetration closure concepts and on alternative piping layouts. Cost data from PSC control rod drive mechanisms, refueling and storage, and other PSC design equipment used in the Base Case were integrated into the Base Case cost compilation, where applicable.

A subcontract was given to United Engineers and Constructors Incorporated, Philadelphia, Pennsylvania for design studies on containment structures and also for assistance on piping and layout studies.

An initial estimate of the Base Case arrangement reactor building was prepared on the basis of cost per cubic foot. This estimate will be used as the basis of comparison in subsequent studies until a more detailed estimate is prepared by United Engineers. The classification of construction cost accounts as defined in Ref. 1 was provided to United Engineers to ensure the proper organization of the building cost estimate.

REFUELING AND ASSOCIATED SYSTEMS

Control and Reactor Service System

The investigation of the Base Case core service, control and reserve shutdown, and contaminated equipment handling systems was completed.

The Base Case core service, viewing, and particle recovery systems are identical to those used for the Fort St. Vrain plant. These devices are used for inspection and retrieval operations within the reactor when it is shut down and depressurized for refueling. The items of equipment are:

1. Reactor viewing device.
2. Core service manipulator.
3. Core service vacuum tool.
4. Reserve shutdown vacuum tool.

The reactor viewing device, core service manipulator, and core service vacuum tool will provide means to observe and perform the following tasks:

1. Retrieve objects such as pieces of broken fuel elements that may have fallen as low as the top of the bottom reflector.

2. Remove a broken control rod cable.
3. Remove a dropped control rod or pieces of a control rod.

The reserve shutdown vacuum tool is used to remove the reserve shutdown material from the core after the material has been released in an emergency.

Viewing Device. The viewing device (see Fig. 1.2) is a commercially available, radiation-hardened television camera with special shielding and cooling developed by Gulf General Atomic. It is mounted on the refueling machine arm, where it can be used to view a dropped fuel element or other abnormality in the core. However, it has no routine function in the refueling operation; full television coverage is neither necessary nor possible.

Core Service Manipulator. The core service manipulator (see Fig. 1.3) is a special tool that can be attached to the fuel handling machine in place of the fuel handling machine head. This change would be made in the hot-service facility. The core service manipulator is a special configuration of standard commercially available PaR Corporation components. It is equipped with accessories provided by PaR, some standard and some specially designed, in accordance with their mechanical standards, for this application. The accessories are provided as part of the manipulator procurement contract. The manipulator is used in the reactor with the refueling machine on manual control; the operator observes this work through the reactor viewing device.

Core Service Vacuum Tool. The core service vacuum tool (see Fig. 1.4) is a special vacuum cleaner developed by Gulf General Atomic for cleaning up small particles of active material in the hot helium atmosphere of the reactor. It consists of a heavy duty vacuum cleaner motor blower unit modified to run in hot helium. This unit exhausts helium from a trapping chamber through a filter and discharges to the ambient reactor atmosphere. On the trapping chamber inlet are a flexible hose and a rigid nozzle. The whole unit is taken into the reactor and positioned by the core service manipulator in view of the reactor viewing device. When loaded with contaminated material, the entire unit is considered expendable.

The motor blower unit for the vacuum cleaner has the same type of motor as the one modified and tested for use in the Peach Bottom vacuum cleaner unit but is larger. Therefore, the techniques of conversion and testing can be regarded as proven.

Reserve Shutdown Vacuum Tool. The reserve shutdown vacuum tool (see Fig. 1.5) is a special, large vacuum tool handled and operated with the auxiliary cask. The reserve shutdown vacuum tool will be used to vacuum out the reserve shutdown material from the core of the reactor in the event that the material has to be released in an emergency. To do this, the fuel and reflector blocks need not be removed. The reserve shutdown vacuum tool consists of three vacuum heads, identical to those used in the core service vacuum tool, mounted on a large disposable container attached to a 20-ft-long probe. The probe locates the hole in the core containing the reserve shutdown material.

To prepare for use of the reserve shutdown vacuum tools, the reactor refueling penetration is opened, and the control and orifice assembly is removed in the same manner as for refueling. The auxiliary cask then transports

Fig. 1.2--Reactor viewing device

Fig. 1.3--Core service manipulator

Fig. 1.4--Core service vacuum tool

Fig. 1.5--Reserve shutdown vacuum tool

the reserve shutdown vacuum tool to the refueling penetration and lowers it into the reactor. After the reserve shutdown material has been vacuumed out from one fuel region, the reserve shutdown vacuum tool is transported by the auxiliary cask to the hot-service facility, where the large disposable container full of reserve shutdown material is removed and replaced by an empty container. The reserve shutdown vacuum tool is then used to vacuum out the next fuel region.

Control and Reserve Shutdown Assemblies

The Base Case reactor has 91 control and reserve shutdown assemblies (see Fig. 1.6), each consisting of two control rods with their drive and a separate reserve shutdown unit all in a common housing. These units are identical to similar units used in the Fort St. Vrain plant, except that they contain no orifice mechanism.

Each assembly forms a closure for one of the refueling penetrations of the PCRV, being secured to a flange in the penetration. Alignment of the control rods with the control rod channels in the core is maintained by keys between the drive housing and the penetration wall.

The control rod drive mechanism is located in the top of the control and orifice assembly housing, and a radiation shield assembly is located directly below it. The housing extends down to the lower end of the refueling penetration, where it terminates in a thermal insulator.

The upper sections of the control rod guide tubes and the cable guide tubes form an integral part of the radiation shield. The lower sections of the control rod guide tubes are joined to the upper sections at the lower end of the radiation shield, the joint being made by means of a telescopic, articulating coupling. This coupling allows both lateral and vertical displacement of the reactor core, and at the same time permits the guide tubes to maintain engagement with the top reflector elements.

Connections for the electrical and purge gas systems enter the side wall of the refueling penetrations just above the top of the control assembly through gastight connectors. The connections then pass through the closure plate in the top of the housing, and from there lead to the respective systems. A removable plug is provided in the closure plate to permit insertion of a tool for manual operation of the control rod drive mechanism. The tool engages a worm wheel mounted directly on the cable drum and provides the operator with a means of manually retracting the rods if necessary. The mechanism employs solid-film lubrication throughout; material selection, contact stresses, etc., are all based on data accumulated over many years of testing at Gulf General Atomic and elsewhere. Similarly, electrical insulation conforms to the specific requirements of the inert, low-dielectric-strength environment in which it is required to operate. The control rod supporting cables are of special stainless steel wire construction. The wire is lubricated with MoS₂ during the drawing process.

The reserve shutdown system provides negative reactivity control entirely independent of the control rod system.

The system is composed of 91 storage hoppers filled with refractory poison nodules containing boron. Each hopper contains approximately 12 kg of boron. The hoppers are located within and form an integral part of the control housing. A guide tube, attached to the underside of the hopper, connects each hopper to the core channel; a pressure line from the gas storage system enters the hopper immediately above a rupture disk in the lower end of the hopper.

The rupture disk in the bottom of the storage hopper retains the nodular material in the hopper. The material can be manually released in either a small group of hoppers or a large group of hoppers and allowed to fall into closed-end channels in the core. The first small group to be released introduces a negative reactivity worth of at least 1.5% Δk . The second large group introduces sufficient negative reactivity worth to shut the reactor down to refueling temperature.

The inside of the storage hopper is normally maintained at approximately reactor pressure by a supply of purified helium from the helium purification system. Thus, the shutdown poison is situated in a purified, dry-gas environment at all times. The disk is ruptured by pressurizing the hopper from gas storage bottles located outside the reactor vessel. Sufficient gas storage capacity, together with an arrangement of control valves, will ensure that all the required disks are ruptured simultaneously. The elapsed time between actuation of the release control for the first small group of hoppers and filling of the core channels with the nodules will not exceed 5 sec.

Contaminated Plant Equipment Handling and Service Systems

Because the Base Case reactor must be consistent with Fort St. Vrain technology, a hot-service facility has been provided, but the plant design will be adaptable to a philosophy of maintenance that is more likely to be typical of future large plants.

In the advanced maintenance philosophy, control rods and circulators would be returned to the manufacturer for reconditioning and to be decontaminated or disassembled in hot-service facilities at his plant. When reconditioning has been completed, each unit would be tested in the manufacturer's hot-helium test facility before being returned to the plant. This procedure utilizes the manufacturer's capability for producing and reworking parts and provides for testing in the facilities of the manufacturer which are much too expensive to be needlessly duplicated. The hot-service facility and its tooling then becomes an investment by the manufacturer and not a necessary part of each power plant.

If at some time in the future a utility or group of utilities has sufficient HTGRs to justify investment in a central maintenance facility, the design of the individual power plant for offsite maintenance of control rods and circulators would still be suitable.

Large items that characterize off-line refueling mechanisms will still have to be dealt with at the site if this type of machine survives. To date, the activity levels of such equipment at Dragon and Peach Bottom have been quite low, and there is reasonable hope that much simpler facilities than those at Fort St. Vrain will be adequate.

Fig. 1.6--Control and reserve shutdown assembly

Circulator and circulator shutoff valve removal will be a routine requirement, and a handling cask moved by the main crane will be specified for the Base Case Plant.

Since steam generator installation and removal is an abnormal operation, it will be provided for with as little modification of the optimum plant design as possible. Rented equipment similar to that used to install heavy components in water-cooled reactor plants will be specified both for the initial steam generator installation and any subsequent replacement operations.

The Base Case hot-service facility is considered to be practically the same as that for Fort St. Vrain, which consists essentially of a shielded room to which certain radioactive equipment can be transferred for service and decontamination by means of remotely controlled equipment.

The facility is composed of a 10- by 10-ft work area with an 8- by 10-ft adjacent pit (see Fig. 1.7). The pit depth is 31 ft from the work area floor, and the ceiling is 25 ft above the work area floor. Most work is done on the service platform, which may be set at various levels in the pit. Operations in the facility are viewed, using binoculars if required, through two lead glass windows located in the end of the facility, away from the pit and above the work area floor. Mirrors are used to see into the pit and to see the sides of equipment away from the windows. All remote controls for the manipulator and hoist are located at the windows outside the facility. Because of the size of the hot-service facility, only one item of the equipment (see list below) can be serviced at a time. However, since a spare circulator assembly will be available, there will be no requirement for circulator servicing during refueling or soon thereafter. Radiation levels up to 1000 r/hr, depending on the geometry of the equipment, may be handled remotely as a routine operation.

Decontamination is accomplished by vacuum cleaning followed by steam or hot-water washing and hot-air drying. Equipment for injecting detergent into the steam or hot water is provided. The spray nozzles and brushes have special handles so that they can be used by the manipulator or by hand.

The following items of radioactive equipment can be decontaminated, repaired, and/or prepared for disposal in the hot-service facility:

1. Control rod and reserve shutdown assemblies.
2. Helium circulators.
3. Fuel handling machine.
4. Auxiliary transfer cask.
5. High-temperature filter-absorbers.
6. Isolation valves.
7. Refueling sleeves.
8. Storage well shielding adapters.
9. Spent fuel container.
10. Fuel shipping cask.

Hot-service facility equipment includes installed items such as the manipulator, hoist, shielded viewing windows, personnel access hatch, and service platform. General-purpose portable equipment includes a vacuum system, steam and hot water decontamination system, and supplementary electrical

Fig. 1.7--Hot service facility

extensions and hose assemblies for fluids and air. Special equipment for servicing the radioactive equipment listed above includes lifting devices and holding and supporting fixtures.

All fixtures are simple fabrications of low carbon steel with a protective coating to withstand decontamination. Tools include wrenches, shears, and abrasive disk cutters, which are standard electrically or pneumatically powered hand tools. By adapting a manipulator wrist connector, they can be used by the manipulator as well as by hand.

Refueling Systems

The Base Case refueling chart was completed. No extensive orifice changing chart was required, because the pattern of operations is simple and repetitive.

Work on a final topical report entitled An Economic Comparison of Three Refueling Systems Suitable for Large HTGR Power Stations, was essentially completed. The report is currently being reviewed in preparation for publication.

The three alternatives to be evaluated are:

Case I. The optimum use of equipment of the Fort St. Vrain type with no significant redesign. The plant drawings have been completed and the refueling chart prepared.

Case II. The optimum use of a fuel transfer mechanism of the Fort St. Vrain type with sufficient auxiliary equipment that it need not be moved away from a penetration to deposit fuel in the fuel storage.

Case III. The optimum use of an on-line refueling machine of the type developed in the fiscal year 1968 Base Program. Work is in progress on the drawings.

Twelve significant cost options will be compared as shown in Table 1.4.

Table 1.4
REFUELING COST COMPARISON

Cost Column	Case	Plant Units	Orifices	Other Features
1	I	Single	Variable	
2	I	Single	Fixed	
3	I	Dual	Variable	
4	I	Dual	Fixed	
5	II	Single	Variable	
6	II	Single	Fixed	
7	II	Dual	Variable	
8	II	Dual	Fixed	
9	III	Single	Fixed	No fuel shuffling
10	III	Single	None	Fuel shuffling
11	III	Dual	Fixed	No fuel shuffling
12	III	Dual	None	Fuel shuffling

A performance analysis of the Base Case fuel handling system was completed and the results are being reviewed.

STEAM GENERATOR

The task to develop various steam generator penetration concepts continued. Alternative designs for the integral reheat and no-reheat steam generators have been drafted. These arrangements consider a bottom-side penetration for the integral reheat steam generator and a straight-through bottom penetration for the no-reheat cycle. At the end of the report period, the drawings were being reviewed.

Tentative code calculations have been performed for all penetration wall thicknesses and changes resulting from drawing review comments are now being checked.

A study of the reheat steam generator tube and support plate thermal expansion during various operating conditions was made. A flexible suspension structure for the tube bundles has been designed that will reduce thermal stress in the support plates while providing the necessary load-carrying capability for earthquake and other design conditions.

The sizing and general layout of the steam-to-steam reheater was completed. Due to building space and piping considerations, it was determined that the steam-to-steam reheater would be more economically enclosed in three vessels rather than the six vessels originally considered. A design criteria document for the steam-to-steam reheater was prepared, reviewed, and released, and is included in Appendix A.

CIRCULATOR

Base Case Circulator

A study to determine the helium circulator sizing requirements for the Base Case was completed during the report period. The design consists of an axial flow compressor driven by a steam turbine using cold reheat steam in series with the main power plant turbine-generators.

The layout for the Base Case circulator was near completion. The flow paths for the steam turbine and the compressor were revised and the over-all design closely integrated with the Base Case steam generator requirements. The Base Case helium shutoff valve was simplified by eliminating the need for a mechanical actuator.

The circulator was sized from the Fort St. Vrain concept except for scaling factors to compensate for the operating requirements of the Base Case primary coolant system.

Calculation Method. The computer code AXCOMP, which calculates a complete axial flow compressor blading design when given an initial set of conditions, was utilized in a parametric study to establish the axial-flow

compressor configuration for the Base Case. AXCOMP includes several assumptions including the following:

1. Every segment along the radius of the blade must be capable of imparting equal work to the gas and maintaining a constant axial throughflow velocity. This is known as a free vortex machine.
2. The gas had no radial velocity during its traversal of the blade.

The first step in the design process is to determine the pressure rise required of the compressor. The design pressure rise is then adjusted for diffuser losses. From this modified pressure rise and an assumed polytropic efficiency, the corresponding temperature rise and thus the work required from the blades is determined.

The blade frictional and boundary layer losses are calculated as a function of the diffusion factor, which indicates the extent of boundary layer buildup on the suction surface of the blade.

The AXCOMP code can be used for single-stage or multistage machines, either rotor-stator or stator-rotor blading sequences, determination of blade-friction and boundary-layer losses, alternate blade-taper characteristics, and part-load performance. The code output includes the number of blades, blade twist, aerodynamic loading factor, machine efficiency, power requirements, and blade stresses.

Compressor Aerodynamic Design. The principles involved in the optimization of the aerodynamic design of axial flow turbomachinery for a light gas such as helium follow from an examination of the simplified one-dimensional energy equation, which equates the energy interchange between the blading and the gas to the change in momentum of the gas stream:

$$gJC_p\Delta T = UC_x(\tan \beta_1 - \tan \beta_2) \quad , \quad (1)$$

where g = gravitational constant,

J = mechanical equivalent of heat,

C_p = specific heat at constant pressure,

ΔT = temperature change in gas,

U = blade speed,

C_x = gas axial velocity,

β_1 = relative entering gas angle, and

β_2 = relative leaving gas angle.

The equation is a simplification of the actual design process whereby the technique of radial and stage stacking is used. The energy equation, Eq. (1), is used for a given number of radial stations. As a result, for every radial segment, there is a different blade speed, and thus a different amount of gas deflection required for equal work. The gas properties are then integrated at the compressor outlet.

The efficiencies of the various compressors were determined using the relationship between the diffusion factor, D , and a pressure loss coefficient, $\bar{w}$. This diffusion factor is an indication of the extent of boundary layer growth on the suction surface of the blade. When D is greater than 0.6, the probability of boundary layer separation on this suction surface becomes significant. In the present design, D was held to less than 0.525 to provide adequate design margin with respect to the 0.6 limit, which is based on cascade testing.

Also inherent in the use of the diffusion factor is its relationship to the pressure-loss coefficient and to solidity σ :

$$D = 1 - \frac{V_2}{V_1} + \frac{(\tan \beta_1 - \tan \beta_2)}{2 \sigma \tan \beta_1} \quad (2)$$

and

$$\bar{w} = \frac{2 \sigma}{\cos \beta_2} (0.0035 + 0.00928D + 0.087D^{3.05}) \quad , \quad (3)$$

where V_2 = relative gas leaving velocity,

V_1 = relative gas entering velocity,

σ = blade solidity, the ratio of blade chord length to the spacing between the blades,

$\bar{w} = \Delta P/q_1$, and

q_1 = inlet velocity head.

Note that while increasing solidity decreases individual blade loading, Eq. (2), the total loss is a result of individual losses summed over the entire stage. This effect is shown in Eq. (3). Thus, there is a definite optimum solidity (within the range of aerodynamic stability).

Another comparison is made between Eqs. (2) and (3) for this optimization study. There is a direct relationship between the amount of energy addition due to a change in angular momentum and the diffusion factor. For a fixed energy input to the gas, an increase in blade speed and velocity will decrease the amount of turning of the gas, and thus decrease D . However, these two components of Eq. (1) are affected by other limitations: the blade speed, U , by the stress it imposes on the blades and disks, and the axial velocity, C_x , by the losses associated with accelerating and decelerating the gas in and out of the machine (particularly the latter).

Actually, this is a simplification, for there are also interrelationships between these terms. For example, the mass flow rate through the machine and the axial velocity determine the inlet annulus area. It is the annulus area coupled with the rotational speed that determines the centrifugal stress. Thus, a small value for C_x coupled with a fixed U will give a larger blade stress than a larger value of C_x . The actual design process requires relatively complicated computations, since iterations are required to optimize the maximum allowable stress to the minimum amount of aerodynamic loading.

With the basic configuration determined, a reasonable outer diameter was selected within the limits of mechanical stress and space limitations. A typical study is shown in Fig. 1.8.

Once a reasonable idea of the compressor's physical dimensions is obtained, the aerodynamic optimization can be performed. Typical optimization curves are shown in Figs. 1.9 and 1.10, which give a plot of efficiency as a function of gas throughflow, centrifugal stress, blade solidity, and diffusion factor. The efficiency of the compressor coupled with the diffuser is plotted. Efficiency is defined as the isentropic temperature rise (based on the ratio of leaving pressure to inlet pressure) divided by the actual temperature rise in the compressor. It is set by the design criteria of a constant diffuser exit pressure and the only variable is the temperature rise or power required in the compressor.

Thus, as far as the compressor itself is concerned, an increase in axial velocity reduces the aerodynamic loading on the blades and therefore reduces the pressure loss coefficient. The loss, however, is one of energy, which results from the product of the loss coefficient and the square of the inlet velocity relative to the blades. Therefore, the minimum loss depends on the magnitude of both quantities, and at higher velocities the velocity square term will become predominant.

The effect of solidity on the efficiency is shown in Fig. 1.10. Unfortunately, the optimum solidity could not be selected in the present design because of four factors. First, the optimum solidity dictates a blade and disk combination that would be very difficult to accomplish within the desired stress restrictions. Second, because of space requirements associated with the containment opening and practical diffuser length, a smaller-diameter compressor was chosen compared to what might appear optimum when Figs. 1.8, 1.9, and 1.10 are compared. Third, Fig. 1.11 shows how the turbine is more efficient at higher speeds; however, the compressor blade centrifugal stresses become greater than the desired limit of 20,000 psi if a combination of large diameter and high speed is encountered. A fourth consideration is the blade factor, which was kept below 0.525 in the present design. Therefore, the final design parameters, as listed in Table 1.5 are necessarily a compromise of many complex, interacting factors.

Only a single-stage compressor was considered in this study, since it has previously been determined that the complexity of a multistage machine outweighs its possible small efficiency increases with the present cycle requirements.

Reheat Series Steam Turbine Drive

The Base Case circulator drive is a reheat series steam turbine similar in concept to that employed in the PSC plant. Again, a single-stage machine has been used for this Base Case for simplicity and reliability. The Base Case steam conditions are given in Table 1.6.

Calculational Method. The computer code DSTURB, which calculates a complete axial flow steam turbine blading design when given an initial set of conditions, was used to establish the axial-flow steam turbine configuration

Fig. 1.8--Compressor sizing study, 1000 Mw(e), $P_{in} = 537$ psia, $\Delta P = 13.0$ psi, $T_{in} = 605^\circ\text{F}$, $\dot{m} = 410$ lb/sec

Fig. 1.9---Compressor sizing study, 1000 Mw(e), $P_{in} = 537$ psia, $\Delta P = 13.0$ psi, $T_{in} = 605^\circ\text{F}$, $\dot{\omega} = 410$ lb/sec

Fig. 1.10--Compressor sizing study, 1000 Mw(e), $P_{in} = 537$ psia, $\Delta P = 13.0$ psi, $T_{in} = 605^\circ\text{F}$, $\dot{m} = 410$ lb/sec

Fig. 1.11--Reheat steam turbine sizing study, $P_{in} = 818 \text{ psia}$, $T_{in} = 723^\circ\text{F}$, $\dot{m} = 284 \text{ lb/sec}$

Table 1.5

BASE CASE CYCLE PARAMETERS
(Plant Rated Conditions)

Primary Coolant System:

Number of circulators	6
Helium flow through each circulator	410.3 lb/sec
Helium temperature at circulator inlet	605°F
Primary system operating pressure	550 psia
Primary system pressure drop	13.0 psi

Helium Circulator Turbine Drive (Reheat Steam):

Number of steam turbines	6
Steam inlet pressure	818 psia
Steam inlet temperature	723°F
Steam discharge pressure	624 psia
Approximate power margin via bypass flow	7%
Steam flow, lb/sec per turbine	284

Table 1.6

BASE CASE MAIN HELIUM CIRCULATOR DESIGN PARAMETERS

	<u>Compressor</u>	<u>Turbine</u>
Fluid	Helium	Steam
Speed, rpm	7000	7000
Inlet temperature, °F	605	721
Inlet pressure, psia	537	795
Outlet pressure, psia	550	628.6
Mass throughflow, lb/sec	410.3	284
Axial throughflow velocity, ft/sec	520	250
Efficiency (including diffuser), %	80.3	85.5
Tip diameter, in.	37.38	23.46
Hub diameter, in.	24.84	19.5
Hub-to-tip ratio	0.66453	0.831347
Number of stages	1	1
Number of rotor blades	31	67
Number of stator blades	33	43
Rotor hub solidity	1.95	2
Stator hub solidity	1.75	2
Diffusion factor (comp. rotor)	0.524	---
Camber (turbine rotor)	---	5.37
Blade centrifugal stress, psi	19,750	6,265
Rotor bending stress, psi	4,900	5,200
Stator bending stress, psi	4,700	4,600
Axial thrust, lb	12,300	24,900
Diffuser:		
Area ratio, inlet/outlet	0.25	0.333
Efficiency, %	80	35

for the Base Case. The compressor power requirements determined the basis for the computer study. The DSTURB code is similar to the AXCOMP code in that:

1. Every segment along the radius of the blade must be capable of extracting equal work from the steam and maintaining a constant axial throughflow velocity for every segment along its radial length.
2. The steam has no radial velocity during its traversal of the blade.

The first step in the design process is to determine the enthalpy drop per stage from the input values of specific work, and the number of stages. With this information, the cross-sectional annulus area for each stage of the turbine is calculated. This is done with four options for the change of diameter: (1) constant outer diameter, (2) constant mean diameter, (3) constant inner diameter, and (4) constant blade height. The blade geometry (such as camber, stagger angle, solidity, and blade profile) can then be determined. Optional blade profile loss calculations are built into the program.

Turbine Aerodynamic Design. The optimization involved in the aerodynamic design of an axial flow turbine is not as critical as that for the compressor. The compressor flow is unstable owing to the adverse pressure gradient. This tends to separate the boundary layer formed on the suction surface of the blade. In the turbine, however, with its positive pressure gradient, the boundary layer is suppressed and swept along with the flow. As a result, there is no separation of the flow. The only limit in the design is in choking the flow in the nozzle.

The two basic types of turbine blading are commonly referred to as impulse and reaction. With impulse blading, the gas velocity and kinetic energy increase as pressure drops in the nozzle; then the kinetic energy is absorbed as useful work in the rotor blades as velocity is diminished at essentially constant pressure. For reaction blading, both nozzle and rotor blades are similar in shape. The cross-sectional area in the direction of gas flow is continually reduced, so both fixed and moving blades act as expanding nozzles and the energy is extracted as velocity increases and pressure decreases.

Reaction blading is more effective for lower gas velocities and is less sensitive to variations in the design velocity ratio. For free vortex flow, such as that used in the DSTURB code, reaction varies with radial distance, being lower at the hub. Impulse blading, on the other hand, produces less discharge loss caused by the leaving velocity and swirl effects.

The turbine blade design selected for the Base Case is predominantly of the impulse type. The degree of reaction that could be incorporated was restricted by the resultant axial thrust loads that could be accommodated by the bearing system as discussed later.

Previous studies had shown that blade solidity had only a small effect on efficiency. The greatest restraining factors on this turbine design were axial thrust, speed limitation, and blade camber. Axial thrust at design

conditions was limited to approximately 25,000 lb in order to remain within the bearing technology capabilities considering the geometry requirements. Speed was limited by the stress restrictions of the compressor.

Blade camber is a function of the steam entering and leaving angles. For the configuration being investigated, a camber value less than 5.4 was required to keep these angles within the range of known performance characteristics.

The diffuser configuration for this turbine concept is considered to be relatively inefficient, primarily because of the geometrical requirements of turning the steam flow 180 degrees. However, this is not too serious, considering that the increased enthalpy from the losses can still be partially utilized by the main turbine.

Figures 1.11 and 1.12 illustrate some of the pertinent parameters that were considered for this study. The Base Case design parameters are given in Table 1.5.

Superheat Steam Turbines

A design criteria document for superheat-steam-turbine-driven helium circulators was reviewed and has been issued (see Appendix B).

Initially, three superheat steam turbine cases were being considered: one with the circulator drive turbine located before the superheater and two having turbine after the superheater as shown in Table 1.7; however, case 2 was dropped because a main steam turbine was unavailable for these conditions. Steam turbine drives for cases 1 and 3 are being sized.

Table 1.7
SUPERHEAT STEAM TURBINE CIRCULATOR DRIVES
(100% Load Steam Conditions)

	Case 1 After Superheater	Case 2 After Superheater	Case 3 Before Superheater
Turbine flow rate, lb/sec	277.5	280	277
Inlet pressure, psia	3225	2465	4205
Outlet pressure, psia	2515	1946	2880
Inlet temperature, °F	1005	1004	850

A case 1 turbine seems feasible by reversing the direction of steam flow relative to the Base Case, but the bearing thrust loads and pressure containment requirements of case 3 present formidable problems that have not yet been resolved.

Fig. 1.12--Reheat steam turbine sizing study, $P_{in} = 818$ psia, $T_{in} = 723^\circ\text{F}$, $\dot{m} = 284$ lb/sec

Extraction Steam Turbine

Design criteria for a parallel extraction steam-turbine-driven circulator are currently being reviewed. The sizing of the turbine was completed. A two-stage velocity-compounded impulse turbine was considered initially, since the disk sealing and pressure differential requirements would be simplified; however, it was determined that supersonic nozzle velocity would be required to supply the necessary power. A modified two-stage turbine was selected with basically an impulse first stage and utilizing considerable reaction in the second stage. Of course, this blading combination is mechanically more complex than a velocity-compounded machine, but the reaction feature does produce slightly higher efficiency as a compensating factor. The layout of the parallel extraction turbine is now in progress.

Electric Motor Drive

Circulator Sizing. A study to determine the requirements and feasibility of a circulator for electric motor drives was completed during the report period. With six circulators in the system, the designs listed below appear aerodynamically feasible (in order of decreasing efficiency):

3550 rpm	single-stage	mixed flow
1775 rpm	single-stage	centrifugal
3550 rpm	two-stage	axial
3550 rpm	single-stage	axial
3550 rpm	single-stage	centrifugal

The 3600 rpm single-stage centrifugal circulator design with variable control vanes appeared to be most capable of meeting the size limitations, control requirements, layout problems, and minimization of research and development cost that had been set up as design objectives. Additional details regarding the selection were reported in part in the previous quarterly report (GA-9227).

Design criteria for the electric-motor-driven circulator was revised in accordance with the circulator selection and released for information (see Appendix C). The corresponding design specification (see Appendix D) was approved, released, and used by outside manufacturers to prepare and submit recommendations for a paid design proposal for a complete unit. Westinghouse Electro-Mechanical Division, Cheswick, Pennsylvania, will investigate the concept in detail, perform a circulator and motor design study, and submit a design proposal for an electric-motor-driven circulator.

Machine Layout. A typical example of an electric-motor-driven circulator layout, the "Hinkley B" machine, is shown in Fig. 1.13 (Ref. 3). This is a 7000-hp horizontal machine featuring a common shaft for the impeller and motor and with flow control by variable inlet guide vanes. The motor cavity lies within the walls of the PCRv, and its external cover acts as the primary closure. No leaktight secondary closure is provided, but a limited clearance barrier heavy enough to withstand the reactor pressure is located between the motor and the impeller, so as to severely limit leakage in case of a primary closure rupture.

Fig. 1.13--Typical electric-motor-driven circulator layout

A small flow of primary coolant takes place through the barrier clearances during reactor pressure variations. Thermal insulation mounted on the reactor side of the barrier limits heat leakage to the motor cavity. Where the shaft crosses the barrier, a labyrinth is provided. In order to prevent the migration of oil vapor or oil-laden gas toward the reactor enclosure, primary coolant taken from the circulator exhaust flows through the labyrinth toward the motor cavity. At the outlet from the labyrinth, this buffer flow is bled off, sent to an external filter, and returned to the blower inlet.

Hydrodynamic oil bearings are used, in conjunction with an external lubrication system. According to the designers, forced lubrication was dictated by the horizontal shaft orientation, whereas oil bath lubrication is considered adequate for a vertical machine of this size. An interesting feature of the Hinkley B design is the isolation system. This consists of a high-pressure dome and a movable seal ring, which can be pulled against the penetration flange, thus allowing a complete motor and circulator unit to be isolated and removed without depressurizing the reactor. The presence of the closure dome unfavorably affects the flow path, which is in itself rather tortuous, since it is based on peripheral intake and axial discharge. This arrangement, unusual for a centrifugal machine, increases the impeller overhang, but it permits the variable guide vane mechanisms to be housed within the motor cavity, where cooling and lubrication can be provided.

The designers of the Hinkley B circulator have also studied vertical shaft arrangements, such as the one shown in Fig. 1.14 (taken from Ref. 4). The over-all design is similar to the previous one. Oil bath bearings are adopted, consistent with the vertical shaft orientation, and the flow path features axial intake and peripheral discharge. With this arrangement, the operating mechanisms for the variable-inlet guide vanes have to be located in an area that cannot be easily protected from the severe primary coolant environment. Also, the flow path is not as streamlined as in most axial intake designs, because of the presence of the inlet guide vanes and the isolation dome.

The arrangement shown in Fig. 1.14 could be adopted, with some modifications, for the Base Program circulator. The main change would be the addition of a double containment with the interspace pressurized by clean helium. The isolation dome could be eliminated, and a shaft shutdown seal (such as is used in the Peach Bottom and PSC circulators) should be introduced. The bearing and seal design might require some modification to allow for the direction of gravity, which is the reverse of that shown on Fig. 1.14, in the Base Program application.

As previously pointed out, the location of the operating gear for the variable vanes in an area surrounded by the primary coolant is an important disadvantage of the arrangement of Fig. 1.14. Dry helium at 600°F is a difficult environment, and it would be highly desirable to locate the actuating mechanisms in an area that can be better protected from this environment. It is not easy to reach this objective if variable-inlet guide vanes are used in conjunction with axial intake and peripheral discharge. However, if outlet vane control is used, the vane operating mechanisms can be housed in the motor cavity (or in a compartment intermediate between the motor cavity and the reactor) where cooling and lubrication can be maintained. To illustrate this

Fig. 1.14--Typical circulator with vertical shaft arrangement

alternative, the layout shown in Fig. 1.15 was developed at Gulf General Atomic. The suppression of the inlet guide vanes and the isolation dome allows considerable streamlining of the flow passages and the operating gear for the variable outlet guide vanes is housed in an area well protected from the primary coolant environment. The arrangement of Fig. 1.15 also illustrates another alternative, i.e., the adoption of separate shafts for the motor and impeller with a coupling between. The motor and associated cooling coils lie in the cavity between the primary and secondary closure. The impeller bearing system is part of the primary closure, and a clean-helium-buffered seal is provided at the reactor end of the impeller shaft.

The arrangements of Figs. 1.14 and 1.15 by no means cover the range of possible layouts, but they illustrate some important alternatives, and form a basis for further studies on the design of the electric-motor-driven circulator.

Cost Considerations. Preliminary investigations have been carried out with various potential vendors. Westinghouse, Allis Chalmers, and Howden & Company have expressed interest and provided rough estimates. Cost data from these three sources, concur in suggesting a price evaluation of about \$900,000 $\pm$ 100,000 per machine. This evaluation is quite rough, and a closer estimate will be obtained by recourse to paid proposal work; however, it can be safely used as a basis for a preliminary economic balance of sufficient accuracy to indicate whether electric motor drive of helium circulators is economically attractive or not. In this balance, the other term of comparison is the steam turbine drive, as used for the PSC reactor. According to information developed by Gulf General Atomic the expected cost for a steam-turbine-driven Base Program circulator is \$450,000.

Special Cases

A circulator is being designed for the reactor-only PCRV concept. The compressor and turbine must be proportionally more powerful to produce the increased pressure differential which is 2.5 psi (19.2%) above that required by the Base Case. The compressor flow paths are also affected by this configuration because of the annular helium discharge.

Design Criteria Summary

The design criteria variations from the Base Case for four plant cycle options have been determined. The plant cycle options include: (1) separate secondary containment, (2) steam-to-steam reheat, (3) no-reheat, and (4) reactor-only PCRV. Therefore the design criteria applicable to all options to be studied during fiscal year 1969 have been established and documented. Tables 1.8 and 1.9 summarize the steam turbine and helium compressor design parameters that have been determined for various circulator drive and plant cycle options.

REACTOR PLANT ARRANGEMENT

Base Case Arrangement

Final layout drawings of the Base Case arrangement were completed and issued. The Base Case plant arrangement is based on a multicavity PCRV (see

Fig. 1.15--Alternative circulator arrangement developed at Gulf General Atomic

Table 1.8

PRELIMINARY STEAM TURBINE DESIGN PARAMETERS FOR MAIN HELIUM CIRCULATOR DRIVES
(100% Load)

Parameter	Option				
	Base Case Reheat ^a	Steam-steam Reheat	No Reheat	Reactor-only PCRVR	Turbine After Superheater
Speed, rpm	7000	7000	7000	7350	7000
Inlet temperature, °F	721	690	697	731	1001
Inlet pressure, psia	795	695	715	830	3144
Outlet pressure, psia	628	550	581	631	2517
Mass throughflow, lb/sec	284	289	325	280	277.5
Axial flow velocity, ft/sec	250	250	275	250	240
Efficiency (including diffuser), %	85.5	86.7	86.2	85.8	84.0
Tip diameter, in.	23.46	24.78	23.95	24.14	21.59
Hub diameter, in.	19.5	20.52	19.50	20.52	20.40
Hub-to-tip ratio	0.8313	0.828	0.8143	0.850	0.9447
Number of stages	1	1	1	1	1
Number of rotor blades	67	61	53	71	93
Number of stator blades	43	47	47	47	79
Rotor blade camber (maximum)	5.37	4.99	4.38	5.49	5.69
Blade centrifugal stress, psi	6265	7125	7145	6550	3650
Rotor blade bending stress, psi	5200	4800	5100	5000	2400
Stator blade bending stress, psi	4600	4800	4900	5100	5000
Axial thrust (downward), lb	29,714	33,561	32,207	36,129	11,283 ^b

^a-Also used for separate secondary containment option.

^b-Flow direction reversed through turbine.

Table 1.9
PRELIMINARY DESIGN PARAMETERS FOR MAIN HELIUM
CIRCULATOR AXIAL COMPRESSORS
(100% Load)

Parameter	Option	
	Base Case ^a	Reactor-only PCRv
Speed, rpm	7000	7350
Inlet temperature, °F	612	591
Inlet pressure, psia	537	534.5
Outlet pressure, psia	550	550
Mass throughflow, lb/sec	410.3	397.8
Axial flow velocity, ft/sec	520	575
Efficiency (including diffuser), %	80.3	79.8
Tip diameter, in.	37.38	35.99
Hub diameter, in.	24.84	24.84
Hub-to-tip ratio	0.66453	0.69018
Number of stages	1	1
Number of rotor blades	31	31
Number of stator blades	33	33
Maximum diffusion factor	0.524	0.532
Blade centrifugal stress, psi	19,750	19,090
Rotor blade bending stress, psi	4900	4800
Stator blade bending stress, psi	4700	5000
Axial thrust (upward), lb	23,087	23,727

^aAlso used for: (1) separate secondary containment, (2) steam-steam reheat, (3) no-reheat, and (4) turbine after superheater.

Fig. 1.16) having six steam generators using gas reheat, as well as six helium circulators driven by cold reheat steam. For this arrangement, AEC standard plant conditions have been assumed for site locations, wind conditions, etc. For the most part, Public Service of Colorado (PSC) technology and criteria have been used.

The areas of plant arrangement that were investigated include fuel storage, hot-service facility, steam piping missile protection, fuel shipping, and overhead crane coverage. The removal of a steam generator has been considered; however, because of the weight (approximately 250 tons), provision will be made for a temporary handling structure to be used and the steam generator will be taken out through the north end of the reactor building. The building height provided for fuel handling is adequate for this operation.

Other areas were not extensively investigated but are adaptations of previous studies that are comparable to the outline of the Base Case arrangement.

Turbine Building Layout. For the purpose of the Base Program studies, the turbine building is assumed to be uniform within the scope of the alternatives considered; for this reason, only reactor building drawings are presented for the Base Case. Orientation of the turbine building is west of the reactor building separated by an auxiliary bay that houses ventilation equipment, control room/controlled access areas, and steam piping. The centerline of the turbine is on a north-south axis, and the distance between PCRV and turbine centerlines is 164 ft.

Reactor Building Layout. The main reactor building width is determined by the PCRV diameter and the wire-winding clearance; the building length is determined by the PCRV diameter, wire-winding space, steam piping header diameter, crane coverage of the fuel storage wells, and the fuel shipping access hatch. A combination of the PCRV height and the steam piping below it determines the height of the refueling floor. The space between the refueling floor and the reactor building roof is determined by crane clearance for the handling of the refueling machine, the crane depth, and the roof truss depth.

The PCRV is prestressed by wire-wrapping, and provision has been made for rewiring the wire-wrapping, if the need should ever arise, by temporarily removing two vertical building columns to allow the tensioning machine to operate. During this time, the building overhead crane must not be used in the reactor area, as its operation requires full crane runway support. By assuming column removability, the width of the reactor building (crane span) has been considerably reduced. It is assumed that the reactor building steel can be so designed that wind bracing is concentrated in bays other than those adjacent to removable columns. If structural design studies prove that this accommodation is not feasible, succeeding layouts will provide the larger building that will be required.

PSC practice employing a structural steel building with steel siding has been used in this arrangement. Fireproofed structural steel construction or solid concrete wall construction are other possibilities that could prove more desirable. An evaluation of these construction types will be made prior to the reference design layout.

The access control area shown for this arrangement is shown in the auxiliary bay. If missile protection is not required for the access area, it is felt that these areas may be housed in a less-expensive type of structure and still afford the capabilities presently shown.

Fuel shipping can be done by either rail or truck. Although truck transport is specified, a rail car is shown for clearance purposes.

The systems equipment shown is based on Base Program layout criteria drawings. There are unused areas in the system section of the building that result from equipment deletions. If no new system requirements develop before an optimized plant layout is made, this space may be rearranged to advantage.

Although they do not appear on the drawings, the reactor building is assumed to have pressure relief and louvres similar to PSC.

Fig. 1.16--General arrangement for the Base Case

Control Room Layout. It is a requirement for the control room layout that the walls provide sufficient shielding for continuous occupancy following a PCRV rupture accident or similar occurrence. Access to the control room under these circumstances will also be provided, since special ventilation features will prevent contaminating helium from entering the control room and access passages.

Secondary Containment Arrangement

A study of the multicavity secondary containment plant arrangement was made, and the plant arrangement drawings for this study are nearing completion. A preliminary plant containment design specification for use with the multicavity reactor plant with separate containment was completed and issued for review.

Reactor-only PCRV Arrangement

A study of the reactor-only PCRV plant arrangement was made, and the plant arrangement drawings for this study are nearly complete.

Steam Piping

A study of the Base Case gas-reheat steam piping with a cold reheat steam-turbine-driven circulator, including a steam piping flexibility analysis, was completed. The analysis confirmed that the piping arrangement was acceptable from the standpoint of thermal expansion and flange loading. A study was also made for the gas-reheat, electric circulator drive concept, including the piping layout.

Layout drawings of steam piping for the after-superheater circulator drive system, the steam-steam reheat systems, the no-reheat system, and the gas-steam reheat system for the reactor-only PCRV were completed and are in the process of flexibility analysis.

Equipment Installation and Service

A study was made on the general philosophy of the major installation and service operations that influence the plant layout and design. The mechanical concepts involved in circulator steam piping missile shield removal, wire-winding and rewinding, steam generator installation and removal, circulator and shutoff valve handling, and steam generator tube plugging were investigated.

REACTOR CONTROL

Part-load operating characteristics were determined for the Base Case HTGR power plant. As might be expected, these characteristics are quite similar to those of the Fort St. Vrain station. Data were also compiled for determining the dynamic response of the steam generator for the Base Case. Several computer runs were made, and comparison of typical response characteristics from previous studies for PSC revealed that the Base Program steam

generator dynamic model is somewhat more responsive to upsets than the Fort St. Vrain model. The primary difference in the response is apparently due to lower tube heat capacities per megawatt of power than for PSC.

The control system requirements for the Base Case and the no-reheat cycle case are being investigated. Work is continuing on the evaluation of the other plant configurations and corresponding control concepts (i.e., electric circulator drives, and two alternative locations of circulator turbines, all as variations on the Base Case design).

A study of the dynamic behavior of the Base Case gas reheat steam generator with and without reheat attemperation (using a linear model developed for Fort St. Vrain) indicates that controlled attemperation will yield the following major benefits:

1. It will decouple some of the effects of main steam temperature on reheat steam temperature.
2. It will reduce the tuning sensitivity and simplify the reheat steam temperature and reactor power controllers.
3. It will provide additional over-all plant control stability margins in all three major plant control loops (i.e., main steam, reheat steam, and throttle pressure).

SYSTEM ANALYSIS

Base Case Primary System Pressure Losses

Primary circuit pressure losses for off-design conditions were evaluated for the Base Case based on the allowances set forth in the previous quarterly report (GA-9227). The projected pressure losses are applicable for a variety of primary conditions including the normal operating range of the plant and emergency cooling. The estimated pressure losses for the reactor core and plenums and the pressure losses in the steam generator loop are shown separately in Fig. 1.17 in order that estimates may be made of the total primary system pressure loss with one or more loops operating. The pressure losses shown are for constant inventory primary system conditions. To adjust for other conditions, these values can be multiplied by the ratio of the average helium density at the design conditions to the average density under consideration. The change in the slope of the pressure loss lines result from a change from turbulent to laminar flow with decreasing values of weight flow.

The relationships were based on a total allowable pressure loss of 6.0 psi for the reactor core and plenum and 7.0 psi for the steam generator and ducting loops. The equations have the form:

$$\Delta P = \text{constant} \times \text{density ratio} \times (\text{flow ratio})^{\text{exponent}}$$

For full inventory calculations, the density ratio of the primary system is unity and the constant is equal to the allowable pressure loss. The value of the helium flow exponent depends on the absolute magnitudes of the flow

Fig. 1.17--Primary system pressure losses. Constant inventory conditions are assumed; to adjust for other conditions, multiply pressure losses by density ratio as in appendix

exponents for friction and entrance and exit losses as well as the relative contributions of the frictional and square-loss pressure drops. For the turbulent Reynolds number range of interest in the reactor core, the Blasius friction law applies ($f = 0.316 \text{ Re}^{-1/4}$), and the helium flow exponent becomes 1.75. The contribution of other losses that vary with the square of the helium flow results in an over-all or equivalent flow exponent of 1.79. In the laminar flow region, the expected over-all flow exponent for the reactor core and plenum is 1.1.

The expected flow exponents for the steam generator and ducting are based on Moody (Ref. 5) and Grimison (Ref. 6) friction data for the ducting and steam generator tube banks, respectively. Flow resistance data for transitional and laminar flow are given in Ref. 7.

1. Core and plenum pressure-loss relationships:

<u>Core Flow Range</u>	<u>Equation</u>
3.5% to 100% (turb. flow)	$\Delta P_c = 6.0 \left(\frac{\% \text{ core flow}}{100} \right)^{1.79} \left(\frac{\bar{\rho}_o}{\bar{\rho}} \right)$
0% to 3.5% (lam. flow)	$\Delta P_c = 0.01494 \left(\frac{\% \text{ core flow}}{3.5} \right)^{1.1} \left(\frac{\bar{\rho}_o}{\bar{\rho}} \right)$

2. Steam generator and ducting pressure-loss relationships:

<u>Steam Generator Flow Range</u>	<u>Equation</u>
1.3% to 100% (turb. flow)	$\Delta P_e = 7.0 \left(\frac{\% \text{ flow}}{100} \right)^{1.95} \left(\frac{\bar{\rho}_o}{\bar{\rho}} \right)$
0.3% to 1.3% (trans. flow)	$\Delta P_e = 0.00147 \left(\frac{\% \text{ flow}}{1.3} \right)^{1.35} \left(\frac{\bar{\rho}_o}{\bar{\rho}} \right)$

Where $\bar{\rho}_o$ = full inventory average helium density at design conditions, and
 $\bar{\rho}$ = average helium density.

Secondary System Parameters

The considerations involved in the selection of secondary system parameters, including pressure losses, were compiled and summarized for the gas-steam reheat cycle, no-reheat cycle, steam-steam reheat cycle, and separate containment alternatives of the gas-steam reheat cycle. The objective was to define a basis for the various allowances made and to establish data for component designs from which piping layouts and cost compilations could be made for a meaningful economic evaluation of the possible alternatives.

The ground rules that served as the basis of the study are:

1. The pressure losses are to be defined for plant rated load (maximum guaranteed turbine flow), with 2414 psia main turbine stop valve inlet and 544 psia IP turbine inlet conditions.
2. The pressure loss allowances for the piping include the effect of pipe length, changes in area and direction of flow in bends, ells, tees, and applicable fittings, block and check valves, and instrument flow nozzles and connections.
3. The pressure loss allowance for control valve or throttling valves is shown separately as applicable.
4. The steam generator and main steam and feedwater piping pressure losses at the turbine VWO (valves-wide-open) point will be approximately 10% higher than those given in Table 1.10 for rated load. (The turbine VWO flow is assumed to be 105% of the maximum guaranteed flow.) The pressures at all points downstream of the turbine first stage are a direct function of steam flow and are therefore 5% higher at VWO flow.
5. The power required by the circulating water pumps, boiler feed pumps, and condensate pumps is calculated approximately as a direct function of flow, the pumping head being treated essentially as a constant.
6. The boiler feed pump turbine takes its steam supply from the IP-LP turbine crossover.
7. The heat cycle allowances for the change in the variable portion of the station auxiliary electrical power has already been factored into the heat rate calculation. The variable part includes the condensate pumps, the circulating water pumps, and the high-pressure condensate pumps where applicable. The remaining part of the station auxiliary electric power remains practically unaffected by omitting or changing the type of reheat.

Gas-to-steam Reheat Cycle. The selection of the Base Case system parameters and primary system pressure loss allocations were discussed in the previous quarterly report (GA-9227). Within the framework of that discussion, Table 1.10 shows the design allocations made for the steam and feedwater system pressure losses. Figure 1.18 shows a schematic diagram of the secondary circuit.

The pressure losses of Table 1.10 are based on engineering judgement. Modern central-station plants generally employ main steam line pressure drops of the order of 2% to 5% of the throttle pressure level. Previous HTGR plant designs have used a figure of approximately 4%, this having been verified in the past as offering the best over-all plant economy. (Actually, the cost perturbation has been rather slight in the pressure loss range of 3% to 5%). Based on this, item a in Table 1.10 is set at 100 psi, or approximately 4%.

Item b is based on a calculated estimate of the losses for a preliminary design of the Base Case steam generator.

Fig. 1.18--Gas reheat cycle BP1, 2538 Mw(t)

Table 1.10
STEAM AND FEEDWATER SYSTEM PRESSURE LOSSES FOR THE BASE CASE

<u>Item</u>	<u>Pressure Loss (psi)</u>
a. Main steam piping from superheater outlet to HP turbine stop valve inlet	101
b. Economizer-evaporator-superheater	630
c. Piping from BFP discharge to economizer inlet, excluding feedwater throttling valve	75 max.
d. Allowance for feedwater throttling valve	75 max.
e. Cold reheat steam piping from HP turbine exhaust to circulator turbine, excluding speed control valve	25
f. Allowance for circulator turbine speed control valve	20
g. Cold reheat steam piping from circulator turbine exhaust to reheater inlet including any reheat attemperators	9
h. Reheater	45
i. Hot reheat steam piping from reheater outlet to IP turbine inlet	26

Item c, the feedwater line pressure loss, is considered compatible with feedwater pipe layout requirements and is based on a permissible velocity in feedwater lines up to about 30 ft/sec. Velocities of this order have been successfully used in modern power plant design, Ref. 8.

From experience of previous control studies on Fort St. Vrain, item d, the feedwater throttling valve pressure loss of 75 psig, is considered an adequate maximum allowance at rated plant load considering that the flow and steam pressure are controlled by varying the feed pump turbine speed. The throttling valve balances the flow.

Considering the over-all reheat steam system, the pressure losses of items e, g, h, and i, together amount to approximately 15% of the average reheat circuit pressure level. This figure is generally in line with previous PSC experience, although it is higher than the 10% value nominally quoted as a reference number in fossil-fuel reheat cycle design. The principal reasons for the increased pressure loss allowance on the HTGR plant are additional piping resistance associated with the interposition of the circulator turbines in the reheat path, the compactness of the HTGR reheater, the necessity for integrating piping mechanical design with equipment layout and pipe reaction criteria, and the somewhat different economics of the HTGR plant relative to the fossil-fuel plant.

The allowance for the pressure loss across the circulator turbine speed control valve is set at approximately 2-1/2%, or 20 psi maximum, based on previous work on the PSC plant.

The design capacity of the circulator turbine bypass line is set at 40% to 55% of plant rated flow at rated inlet pressure and temperature, based on the following considerations:

1. At 25% plant load, the steam flow required by the circulator turbine is found (from PSC experience) to be approximately one-half of the available HP exhaust flow at that load. The latter flow is a quarter of that at the rated point. The bypass line should have adequate capacity to divert the unwanted flow. The inlet steam at this time is at one-fourth rated pressure and has nearly the same degree of superheat as at the rated point.
2. Approximately 3% steam bypass flow is required at plant rated conditions for design and control margins. The control valve rangeability should therefore permit the necessary turndown.
3. The bypass valve and its piping are sized to maintain a constant pressure ratio of about 1.3 across the circulator turbine, from the rated load down to approximately 25% load.

It was determined on the PSC work that a judicious coordination of these considerations results in an equivalent bypass line flow capacity of 40% to 55% in terms of the plant rated flow, pressure, and temperature conditions, somewhat depending upon how manufacturers' standard valve ratings compare with the calculated requirements.

By way of general reference, a table showing Fort St. Vrain Unit No. 1 parameters is given in Appendix E of this report.

No-reheat Cycle. The no-reheat cycle parameters are based on using a cross-compound (CC4F) turbine-generator in place of the 3600-rpm tandem-compound (TC6F) machine of the Base Case. The cross-compound machine has a 3600-rpm HP-IP turbine on one shaft and an 1800-rpm four-flow LP turbine with 43-in. last-stage blades (LSB) on the second shaft. The 1800-rpm shaft is necessary because LP blades are not commercially available for operation at 3600 rpm with the high exhaust moisture occurring in the last stages of the no-reheat turbine (16% nominal compared to approximately 9% on the Base Case).

Figure 1.19 shows the secondary circuit schematically. The net electrical output for constant reactor thermal power is 959.5 Mw versus 1000 Mw for the Base Case. If the TC6F machine with its high exhaust loss were used, the no-reheat cycle heat rate would theoretically have been 5.8% worse than the Base Case. The choice of the cross-compound turbine, however, results in a reduced exhaust loss in the no-reheat case, and the net worsening of the heat rate relative to the Base Case is actually about 4.2%. The turbine generator gross output is 976.4 Mw versus 1016.5 Mw for the Base Case, with constant reactor power.

For the same reactor thermal power, the turbine throttle flow is 16% higher than that of the Base Case, partly because of a smaller total enthalpy change per pound of steam (in the absence of reheat) and partly because of the 4% increase in cycle heat rate.

Table 1.11 shows the design allocations made for the steam and feedwater system pressure losses for the no-reheat cycle.

Fig. 1.19--No-reheat cycle BP6, 2538 Mw(t)

Table 1.11

STEAM AND FEEDWATER SYSTEM PRESSURE LOSSES FOR THE NO-REHEAT CYCLE

<u>Item^a</u>	<u>Pressure Loss (psi)</u>
e. Steam piping from HP turbine exhaust to circulator turbine excluding the speed control valve at inlet	22
f. Allowance for circulator turbine speed control valve	20
g. Cold reheat steam piping from circulator turbine exhaust to IP turbine inlet	35

^aItems a through d have same allowances as items a through d in Table 1.10.

Items a, b, c, and d follow the same philosophy as for the Base Case (see preceding section). Considering the over-all intermediate-pressure system, the pressure losses of items e and g together amount to 57 psi, or nearly 9% of the average pressure level there. Of this, approximately 40% is currently allotted to item e and 60% to item g, since item g carries steam of higher specific volume, and the geometrical layout has relatively larger uncertainty; the proportion of the distribution may be reviewed at the piping design stage.

The pressure loss allowance for the circulator turbine speed control valve (item f) and the sizing of the circulator turbine bypass line are based on the same considerations as discussed for the Base Case.

The principal effects of the changed secondary system parameters on the design capacity of the equipment and possible changes in building space requirements are discussed in a later section.

Steam-to-steam Reheat Cycle. In contrast to the no-reheat cycle, the last-stage moisture in the steam-to-steam reheat cycle can be kept low enough to enable the more economical 3600-rpm tandem-compound (TC6F-31 in. LSB) turbine generator to be used as in the Base Case. Of the two locations convenient for adding reheat to the cycle (that is, HP exhaust and IP-LP crossover), the crossover with an average pressure of approximately 180 psia has been selected. This permits a lower reheat temperature than the HP exhaust location for acceptable moisture in the last stage (690°F), and provides adequate LMTD in the reheater. Approximately 13% of the boiler main steam output is used as the heating medium on the tube side of the reheater. The steam is desuperheated and condensed, and the condensate is slightly subcooled before being returned to the feedwater circuit by means of the high-pressure condensate pump.

The cycle net heat rate would have theoretically shown 1.2% improvement over a no-reheat cycle if the last-stage exhaust losses were the same in both cases. However, since the TC6F 3600-rpm single-shaft machine with the largest

practical blade has a smaller exhaust annulus area than the CC4F 1800-rpm machine of the no-reheat cycle, the actual heat rate works out to be 0.4% worse than the no-reheat cycle, by virtue of the increased exhaust loss cancelling the gain due to reheating. The price difference between the cross-compound and tandem-compound machines of 1000-Mw capacity is, however, such that it would not pay to resort to cross-compounding in order to save this exhaust loss. (The difference is of the order of \$2.5 million for 1000-Mw machines.) Based on this analysis, it appears that a steam-steam reheat scheme that is practically feasible for the HTGR system does not have a significant potential for cycle efficiency improvement over the straight no-reheat cycle. (The turbine generator gross output is 974.6 Mw versus 1016.5 Mw for the Base Case, with constant reactor power.)

Although the main turbine throttle flow is only 4% greater than that for the Base Case, the steam generator flow is 20% higher than the corresponding Base Case flow. This is because of the flow returned by the high-pressure condensate pump, which must be converted to superheated steam. (The following section shows a comparison of principal secondary system parameters and a discussion of their influence on plant design.)

Table 1.12 shows the design allocations made for the steam-water system pressure losses for the steam-steam reheat cycle.

Table 1.12
STEAM AND FEEDWATER SYSTEM PRESSURE LOSSES
FOR THE STEAM-STEAM REHEAT CYCLE

<u>Item^a</u>	<u>Pressure Loss (psi)</u>
h. IP turbine exhaust to reheater inlet	3
i. Reheater shell side	7
j. Reheater outlet to LP turbine inlet valves	4
k. Connection from main steam line to reheater tube side inlet including the trim valve at inlet	39
l. Reheater tube side	25
m. High-pressure condensate line from reheater to pump suction	3
n. High-pressure condensate line from pump discharge to the high-pressure feedwater line	20 max.

^a-Items a through g have same allowances as for items a through g in Table 1.10.

Items h, i, and j, taken together are set at about 8% of the pressure level on the shell side of the reheater. Items k and l together are set

at approximately 3% of the main steam pressure. Item m will need coordination for obtaining a proper margin between available NPSH from static elevation of the reheater from the HP condensate pump and its NPSH requirement. Item n and any static head required for pumping to the feedwater line will determine the discharge pressure of the high-pressure condensate pump.

Relative Equipment Size and Building Space Requirements. The relative steam, feedwater, condensate, and heat flow rates for equal reactor thermal power for the Base Case, no-reheat, and steam-steam reheat cycles compare approximately in the ratios shown in Table 1.13. These flow ratios together with the pressure losses of Tables 1.10, 1.11, and 1.12 are indicative of how much additional capacity will be required of some of the major equipment affected by the cycle change. The equipment includes main and auxiliary condensers and their circulating water system, feedwater heaters, demineralizers, condensate pumps, boiler feed pumps and their drives, circulator turbines, bypass flash tanks, extraction steam system and their associated piping and valves. The effect of nearly 10% lower HP turbine exhaust pressure in the no-reheat and steam-steam reheat cycles will have an added effect of increasing the volumetric flow requirement of the circulator turbine and related steam piping. The pressure and temperature rating of the flash tank and the intermediate pressure piping will change.

Table 1.13

RELATIVE MASS AND HEAT FLOW RATES
(Base Case = 1.0 for All Flow Rates)

	Base Case	No-Reheat	Steam-steam Reheat
a. Turbine throttle flow	1.0	1.16	1.04
b. Last-stage exhaust flow	1.0	1.12	1.025
c. BFP turbine steam flow	1.0	1.4	1.24
d. Condensate flow	1.0	1.15	1.04
e. Boiler feed pump water flow	1.0	1.16	1.04
f. Steam generator flow	1.0	1.16	1.20
g. Circulator turbine flow	1.0	1.162	1.04
h. Heat rejection to main condenser circulating water	1.0	1.01	1.023
i. Heat rejection to auxiliary condenser circulating water	1.0	1.45	1.11
j. Total heat rejection to circulating water	1.0	1.024	1.03
k. High-pressure condensate flow, fraction of throttle flow	None	None	0.15
l. Heat rate, percentage differential from Base Case	Base	4.2 Worse	0.4 Worse

After allowances are made for the above capacity increases on the basis of constant reactor thermal power, an incremental capability charge for the differential electrical power for the entire plant will be superimposed to arrive at the final cost figures in terms of plants of equal electrical capacity.

Increased building space will be required for the no-reheat cycle for the cross-compound turbine generator relative to the tandem-compound unit of the Base Case, and to a certain extent for the increased flow capacity of the steam and feedwater system equipment in case of both no-reheat as well as steam-steam reheat cycles. The foundations for the twin-shaft cross-compound turbine are likely to have increased height and mass compared to the Base Case machine. Another item requiring consideration during cost comparison is the increased overhead clearance space required for lifting the larger diameter turbine wheel associated with the 43-in. blades and increased crane lifting capacity.

The relative power consumption of major pumps for the three cases is given in Table 1.14.

Table 1.14
RELATIVE PUMPING POWER

	Base Case	No- Reheat	Steam-steam Reheat
a. Boiler feed pumps	1.0 (24,460 kw)	1.152	1.03
b. Condensate pumps	1.0 (1,994 kw)	1.15	1.055
c. Main condenser circulating water pumps	1.0 (4,540 kw)	1.012	1.021
d. High-pressure condensate pump for steam-steam reheater, kw	None	None	1,566

The absence of gas-steam reheating will obviate the necessity for 1000°F chrome steel hot reheat steam piping and the reheater piping penetrations. A bypass to the condenser from the circulator turbine exhaust will be required in place of the hot reheat steam bypass. Emergency situations that produce an abnormal rise in hot helium temperature would require different safety considerations from the Base Case, when there is no reheater ahead of the superheater in the hot-gas path.

The steam-steam reheater and its interaction with the main turbine design and operation would require study, together with the associated controls.

Refinements to Steam-to-steam Reheat Cycle. Since the steam-steam reheat cycle offers little thermodynamic advantage over the no-reheat cycle, it would, at first glance, appear advisable to look into ways of improving it. The thermodynamic loss involved in using live steam for reheating can be reduced by devising a cycle that would use instead the HP turbine exhaust steam, which has already done some work. If the HP turbine heat drop were so chosen that it yielded an exhaust pressure of, say, 1000 psia, the condensing temperature of 544°F at this pressure would permit a hot reheat temperature of only about 550° versus 690°F. To obtain a last-stage moisture comparable to the previous case, the crossover pressure at which reheating is done would have to be lowered from 180 psia to about 90 psia. This lowers the crossover point to the wet region requiring some steam-water separation before reheating, which would add to the equipment cost. The lowering of both the pressure and temperature of reheat generally carries with it a thermodynamic penalty that will offset the gain due to the extraction of the heating medium from a point (viz the HP exhaust) where work has already been obtained from the steam. It is not believed that this approach will bridge any significant fraction of the 4% heat rate differential that exists between the Base Case on one hand and either the no-reheat or steam-steam reheat case on the other. Moreover, the turbine generator with the low 90-psia crossover pressure would become somewhat of a specialty.

In a heat cycle analysis of Ref. 9, Westinghouse has claimed a possible gain of 0.3% to 0.5% in heat rate by reheating the steam in two steps as against the type of one-step reheating considered here. The improvement in the case of the water reactor cycle is from 9751 to 9706 Btu/kw-hr for the Westinghouse parameters shown below.

Reheat Steam Conditions

Pressure	163 psia
Condition before reheating	1195 H, 365°F
Condition after reheating	1272 H, 500°F

Condition of Primary Steam Used as Heating Medium

One-step heating	850 psia sat.
Two-step heating	410 psia wet, first step
	850 psia sat, second step

Condition of Condensate to be Pumped

One-step heating	830 psia, 522°F
Two-step heating	400 psia, 449°F, first step
	830 psia, 522°F, second step

In a two-step reheating, condensate at near saturation temperature would have to be handled from the tube sides of the reheater at two pressures, 950 and 2350 psia. This would require two additional sets of pumps instead of one. Alternatively, the 950-psia condensate could be flashed to the deaerator, but this would require increased boiler feed pump capacity. Because of the already high basic efficiency inherent in the HTGR steam conditions relative to the water reactors, it is possible that the two-step reheating may

not show as much improvement as in the water-reactor described by Westinghouse. Also, because of the higher pressure and temperature levels in the HTGR, the reheater cost may weigh more against the two-step process than in the water reactor cycle.

The steam-steam reheat cycle heat rate can also be improved by allocating a lower pressure loss to the reheater and piping. Their size and cost would thereby increase. It is also possible to improve the heat rate by utilizing some of the cold reheat steam for additional feedwater heating, thereby reducing the amount of steam to be reheated, but not without the cost of another feedwater heater, a larger flow capacity in the feedwater and condensate equipment, and a larger steam generator surface. This arises out of the reduced economizer LMTD. The effect of the exhaust losses on heat rate has already been discussed.

In summary, without the benefit of the results from considerable additional analysis, it is reasonable to say that 1/2% to 1% improvement in heat rate can possibly be realized by increased spending on capital equipment. But it appears unlikely that the net effect would significantly change the comparative over-all economic assessment of the three cycles.

Separate Containment. For the two cases requiring separate containment for enclosing the reactor core and steam generators or separate containment for reactor-only PCRV and steam generators, it is proposed to use approximately the same secondary-side pressure loss allowances as given in Table 1.10 for the Base Case. The possibility is recognized that some of the piping could require somewhat longer runs than those of the Base Case and perhaps some additional valving. There is a chance that this could require a slight increase in flow area. It is believed that this approach in which the secondary-side pressure losses are kept at the same level as the Base Case will provide as reliable an economic comparison as any other approach.

Effect of Reheat-circuit Pressure Loss on Station Heat Rate. While engineering judgement has been used in assigning the preliminary pressure losses in the previous sections, the following discussion is presented for use in economic assessment studies, if the pressure losses need reviewing at a detail stage.

Information supplied by General Electric on the Fort St. Vrain Unit I steam cycle shows that the heat-rate penalty for every 1% addition of pressure loss in the reheat system (comprising the reheater with cold and hot reheat piping) is 0.095%.

A first-order verification of this number can be obtained from entropy increase considerations as in the following calculation for throttling from, say, 700 to 630 psia (10% pressure loss) at 1430 H (which are the approximate mean reheat circuit conditions for the 1000-Mw Base Case):

- a. Pressure loss in throttling 10%
- b. Entropy increase in throttling loss
 (from Mollier diagram) 0.0112 Btu/lbm-°F
- c. Steam flow to condenser 4.96×10^6 lb/hr
- d. Condensing temperature 102°F (562°R)

- e. Killwatts lost to generation (product
of b, c, and d divided by 3413
Btu/kw-hr) 9150 kw
- f. Loss as percent of net rated kilowatts
(1,000,000 kw); this is also the heat
rate penalty, for 10% pressure loss 0.915%
- g. Heat rate penalty per 1% pressure
loss 0.0915%

This calculation is in approximate agreement with the 0.095% figure supplied by General Electric for Fort St. Vrain; it is given here to show the relationship between the heat rate penalty and the product of the entropy increase and the steam flow to the condenser.

A similar calculation for the throttling loss involved in the steam-steam reheating at the crossover pressure shows that for every 1% addition of pressure loss there, the heat rate penalty using the above estimating method would be nearly 0.11%.

An examination of the Mollier diagram shows that the entropy increase for superheated steam for a given fractional pressure loss, say 10%, remains nearly constant (within $\pm 5\%$ to 10%) for typical combinations of pressure and enthalpy commonly encountered in steam cycles: 2500 psia 1460 H, 2000 psia 1460 H, 700 psia 1430 H, 200 psia 1300 H, 30 psia 1200 H.

It therefore appears reasonable to use a round number of 0.10% heat rate penalty for every additional 1% of constant-enthalpy pressure loss for cost assessment studies of the Base Program. This number frequently appears on General Electric generalized turbine performance data sheets for the reheat circuit, and small departures from this, such as $\pm 0.01\%$, will have no significant effect on costs, for comparative purposes.

With the help of the percentage heat rate penalty discussed above, the appropriate evaluated capability and fuel cost differentials can be calculated in accordance with the ground rules of Ref. 6.

The above discussion is not intended to apply to the pressure loss in the feedwater line, the economizer-evaporator-superheater, or the main steam line. Since the steam pressure is normally held constant at the main turbine stop valve, the increased pressure loss imposes an additional cost penalty by virtue of the increase in design pressure of the equipment and piping upstream of the turbine, and the increase in the boiler feed pump discharge head and the horsepower. Information on this cost tradeoff will be developed later.

Gas Reheat Cycle with High-pressure, High-temperature Steam-turbine-driven Circulator

Heat cycle analyses were completed for the gas reheat cycle employing high-pressure, high-temperature steam-turbine-driven helium compressors. Several heat-balance calculations were made in order to evaluate the possible

operating conditions of this concept. The pressure ratio required by the circulator turbine and the control valve is slightly less than that required by the Base Case circulator turbine because of the higher-enthalpy steam; however, because of a higher exhaust pressure, a much larger pressure drop is required, and this results in higher design pressures in the steam generator when the design pressure at the turbine throttle is held at 2400 psig. Higher pressures in the secondary system cause increased costs in the main steam piping, the steam generators, and the feedwater pumps, drives, and piping. In addition, if the temperature at the throttle is held constant at the Base Case design value of 1000°F, the required output from the superheater is about 1070°F. Preliminary cost information shows significant economic penalties when the steam generator pressures are increased to correspond with the pressure drop across the circulator turbine. Even stronger economic penalties are realized when both the steam pressure and temperature at the superheater are elevated as required to yield steam conditions at the main turbine throttle equal to the Base Case cycle (i.e., 2414 psia and 1000°F).

Cycle conditions at the throttle and steam generator are given in Table 1.15 for the cases studied, plus, for reference, the Base Case conditions.

Table 1.15
SUMMARY OF CYCLE CONDITIONS

Condition	Case BP1 (Base)	Case BP8	Case BP8b	Case BP8c
Throttle conditions:				
Pressure, psia	2414	1850	2414	2414
Temperature, °F	1000	936	1000	932
Steam generator outlet:				
Pressure, psia	2515	2515	3220	3275
Temperature, °F	1005	1005	1069	1005
Cycle performance:				
Reactor power, Mw(t)	2538	2538	2538	2538
Net plant output, Mw(e)	1000.0	1000.0	1031.5	1019.9
Net plant efficiency, %	39.39	39.39	40.63	40.18

Preliminary costs anticipated from cost data being developed indicate a cost penalty of at least \$0.5 million more than the Base Case. The economic data tend to favor steam conditions at the main throttle that correspond to the Base Case steam generator design conditions (case BP8). However, the turbine generator manufacturers cannot, in the foreseeable future, offer 3600-rpm units with a net rating of 1000 Mw(e) at throttle pressures substantially below 2400 psig. This was recently confirmed by General Electric, who say that the volumetric flow required at 1800 psia for 1000-Mw output would be prohibitive. Assuming for the moment that the throttle pressure is pegged at a minimum value of 2400 psig because of current turbine design limitations, it is prudent to consider throttle temperature variations on

over-all economics for this cycle. For this circulator turbine arrangement, the over-all economic comparisons with the Base Case plant are closely linked to the relative steam generator costs.

Preliminary costs indicate that the increased performance and capability cost gains of the heat cycle associated with throttle conditions of 2414 psia and 1000°F (case BP8b) will not be sufficient to offset the added costs associated with the 3220-psia 1069°F generator, steam piping, and related feedwater pumps, drives, and piping.

If, however, the throttle temperature is reduced to a point (932°F) that yields a superheater outlet steam equal to that of the Base Case design value, the steam generator cost is significantly reduced from the previous case. Therefore, a compromise is made, and cycle conditions (BP8c) having a 2414-psia throttle pressure and a reduced throttle temperature have been selected for this circulator turbine arrangement.

The evaluated cost for the proposed cycle is \$1 to \$2 million more than the Base Case. The use of Inconel 625 in place of Inconel 800 in the superheater section of the steam generator was evaluated and found to decrease the total evaluated cost by about \$0.5 million for the conditions corresponding to case BP8c.

The cycle parameters for this case are given in Table 1.16. Figure 1.20 is a heat balance diagram for the cycle.

Gas Reheat Cycle with High-pressure Partial-superheat Steam-turbine-driven Circulator

Another possible location for the high-pressure circulator turbine described above is between the presuperheater and final superheater sections of the steam generator. This location would require an intermediate header and the necessary piping between these sections. Although this arrangement complicates the steam generator header and penetration design, the cycle does provide a higher cycle efficiency than the Base Case cycle, with the same temperature and pressure at the superheater outlet.

Figure 1.21 shows the heat balance diagram for the cycle. The allowable pressure drops in the high-pressure sections of the steam generator were divided equally between the economizer-evaporator-presuperheater section and the final superheater. In addition, the cycle was also penalized with additional pressure losses in the steam lines between the steam generator headers and the circulator turbine.

The cycle parameters are given in Table 1.17. The efficiency for the high-pressure series-steam-driven circulator turbine was estimated to be 3% lower than the circulator turbine efficiency used in the Base Case cycle because of increased leakage losses. Further cycle analysis may be necessary after more detailed information becomes available for the circulator turbine, steam generator, and intermediate piping between the evaporator and final superheater.

Table 1.16
HIGH-PRESSURE AND TEMPERATURE CIRCULATOR TURBINE
GAS REHEAT CYCLE PARAMETERS
(Case BP8c)

Over-all Plant Performance:

Gross turbine generator output, Mw(e)	1,036.0
Net plant output, Mw(e)	1,019.9
Net plant efficiency, %	40.18
Net plant heat rate, Btu/kw-hr	8,495

Primary Coolant System:

Reactor gross thermal power, Mw(t)	2,538
He flow through circulators, lb/hr	8,862,000
He temperature at circulator inlet, °F	612
He temperature at steam generator inlet, °F	1,427
Primary system pressure drop, psi	13.0
Primary system efficiency, %	99.45
Primary system operating pressure, psia	550

Secondary Coolant System:

Turbine Generator:

Turbine throttle pressure, psia	2,414
Turbine throttle temperature, °F	1,000
Turbine throttle flow, lb/hr	6,446,700
HP turbine exhaust pressure, psia	644
HP turbine inlet pressure, psia	544
IP turbine inlet temperature, °F	1,000
IP turbine steam flow, lb/hr	6,411,100
LP turbine backpressure, in. Hg abs	2.00
Turbine arrangement	TC6F-31 in.

Regenerative Feedwater Cycle:

Feedwater flow, lb/hr	6,467,500
Final feedwater temperature, °F	335
Number of feedwater heaters	4

Helium Circulator Turbine Drive:

Inlet pressure, psia	3,225
Inlet temperature, °F	1,004
Outlet pressure, psia	2,515
Approx. power margin via bypass flow, %	7

Steam Generator:

Superheater outlet pressure, psia	3,275
Superheater outlet temperature, °F	1,005
Econ-evap, superheater Δp , psi	630
Reheater outlet pressure, psia	570
Reheater outlet temperature, °F	1,001
Reheater pressure drop, psi	45

Fig. 1.20--High-pressure and temperature circulator-turbine gas reheat cycle BP8c, 2538 Mw(t)

Fig. 1.21--High-pressure circulator - turbine gas reheat cycle BP10b, 2538 Mw(t)

Table 1.17

HIGH-PRESSURE CIRCULATOR-TURBINE GAS REHEAT CYCLE PARAMETERS
(Case BP10b)

Over-all Plant Performance:

Gross turbine generator output, Mw(e)	1,045.5
Net plant output, Mw(e)	1,029.3
Net plant efficiency, %	40.54
Net plant heat rate, Btu/kw-hr	8,418

Primary Coolant System:

Reactor gross thermal power, Mw(t)	2,538
He flow through circulators, lb/hr	8,862,000
He temperature at Circulator inlet	612
He temperature at steam generator inlet, °F	1,427
Primary system pressure drop, psi	13.0
Primary system efficiency, %	99.45
Primary system operating pressure, psia	550

Secondary Coolant System:

Turbine Generator:

Turbine throttle pressure, psia	2,414
Turbine throttle temperature, °F	1,000
Turbine throttle flow, lb/hr	6,411,700
HP turbine exhaust pressure, psia	644
IP turbine inlet pressure, psia	544
IP turbine inlet temperature, °F	1,000
IP turbine steam flow, lb/hr	6,377,400
LP turbine backpressure, in. Hg abs.	2.00
Turbine arrangement	TC6F-31 in.

Regenerative Feedwater Cycle:

Feedwater flow, lb/hr	6,432,500
Final feedwater temperature, °F	336
Number of feedwater heaters	4

Helium Circulator Turbine Drive:

Inlet pressure, psia	4,205
Inlet temperature, °F	851
Outlet pressure, psia	2,880
Approx. power margin via bypass flow, %	7

Steam Generator:

Superheater outlet pressure, psia	2,515
Superheater outlet temperature, °F	1,005
Econ-evap, presuperheater Δp , psi	315
Final superheater Δp , psi	315
Reheater outlet pressure, psia	570
Reheater outlet temperature, °F	1,001
Reheater pressure drop, psi	45

Gas Reheat Cycle with Condensing Turbine Circulator Drives

An investigation into the concept of using condensing turbine drives for the helium circulators showed a sizeable cost penalty when compared to the Base Case helium circulator drive and cycle as reported in the previous quarterly report (GA-9227). Cycle parameters were developed to establish the cycle heat balance for an HTGR plant using the condensing turbine circulator drive. The detailed data developed for this case are listed in Table 1.18 for a fixed reactor power along with the Base Case reference conditions.

The final heat-balance diagram is shown in Fig. 1.22 (case BP4). As previously reported, the crossover flow between the IP and LP turbines is smaller than for the Base Case and gives rise to an over-all saving in heat rate of 30 Btu/kw-hr, principally from a reduced exhaust loss.

Gas Reheat Cycle with Electric-motor Circulator Drive

The effect on cycle performance by substituting an electric-motor helium circulator drive for the series-steam-turbine drive in the Base Case heat cycle was evaluated. The cycle parameters for an integral gas reheat cycle using motor-driven circulators are listed in Table 1.19 for a reactor thermal power of 2538 Mw. Helium circulation is provided by a 3600-rpm single-stage centrifugal compressor with an expected efficiency of 77%. Although the efficiency of the compressor is slightly poorer than the axial flow unit used in the Base Case reference cycle, the net plant heat rate is improved slightly (12 Btu/kw-hr), because the high-pressure stage performance of the turbogenerator is better than that of the circulator turbine and its control valve losses. Figure 1.23 is a heat-balance diagram showing the cycle conditions.

The gross turbine generator output must increase in order to supply power to the six motor-driven circulators. The effect of the electric drives on the design, rating, and evaluated cost of other equipment items in the plant affected by this change to the Base Case design is discussed below.

Cost information on electric circuit breakers and auxiliary transformers and incremental cost of the turbine generator have been based on catalog data. The standby diesel generator cost is extrapolated from a vendor quotation.

With the exception that the helium circulators are driven by electric motors instead of turbines, the heat cycle is assumed to be similar in all other particulars to the Base Case consistent with the ground rule that one variable is perturbed at a time. Figures 1.18 and 1.23 schematically show the steam-water circuits.

In conformity with previous HTGR cost analysis methods, the cost comparison has been made for plants of equal thermal output. That procedure results in essentially no change in costs for much of the reactor plant equipment. However, a capability charge for differential electric power is later applied in the comparison, resulting in final cost figures in terms of plants of equal electrical size. This is considered adequate for comparison of relatively similar designs.

Table 1.18
BASE CASE AND CONDENSING CIRCULATOR TURBINE DRIVE PARAMETERS
(Cases BP1 and BP4)

	Base Case BP1	Condensing Turbine BP4
Over-all Plant Performance:		
Gross turbine generator output, Mw(e)	1,016.5	1,019.0
Net plant output, Mw(e)	1,000.0	1,003.0
Net plant efficiency, %	39.39	39.53
Net plant heat rate, Btu/kw-hr	8,663	8,633
Primary Coolant System:		
Reactor gross thermal power, Mw(t)	2,538	2,538
He flow through circulators, lb/hr	8,862,000	8,862,000
He temperature at circulator inlet	612	612
He temperature at steam gen. inlet, °F	1,427	1,427
Primary system pressure drop, psi	13.0	13.0
Primary system efficiency, %	99.45	99.45
Primary system operating pressure, psia	550	550
Secondary Coolant System:		
Turbine Generator:		
Turbine throttle pressure, psia	2,414	2,414
Turbine throttle temperature, °F	1,000	1,000
Turbine throttle flow, lb/hr	6,505,900	6,479,700
HP turbine exhaust pressure, psia	846	644
IP turbine inlet pressure, psia	544	544
IP turbine inlet temperature, °F	1,000	1,000
IP turbine steam flow, lb/hr	6,458,900	6,439,600
LP turbine backpressure, in. Hg abs	2.00	2.00
Turbine arrangement	TC6F-31 in.	TC6F-31 in.
Regenerative Feedwater Cycle:		
Feedwater flow, lb/hr	6,523,100	6,496,900
Final feedwater temperature, °F	330	330
Number of feedwater heaters	4	4
Helium Circulator Turbine Drive:		
Inlet pressure, psia	821	167
Inlet temperature, °F	724	713
Outlet pressure, psia	624	1.23
Approx. power margin via bypass flow, %	7	7
Steam Generator:		
Superheater outlet pressure, psia	2,515	2,515
Superheater outlet temperature, °F	1,005	1,005
Econ-evap, Superheater Δp , psi	630	630
Reheater outlet pressure, psia	570	569
Reheater outlet temperature, °F	1,001	1,001
Reheater pressure drop, psi	45	45

Fig. 1.22--Condensing circulator turbine drive BP4, 2538 Mw(t)

Table 1.19
MOTOR-DRIVEN CIRCULATOR CYCLE PARAMETERS
(Case BP9)

Over-all Plant Performance:

Gross turbine generator output, Mw(e)	1,065.9
Net plant output, Mw(e)	1,001.1
Net plant efficiency, %	39.45
Net plant heat rate, Btu/kw-hr	8,651

Primary Coolant System:

Reactor gross thermal power, Mw(t)	2,538
He flow through circulators, lb/hr	8,862,000
He temperature at circulator inlet	612
He temperature at steam generator inlet, °F	1,427
Primary system pressure drop, psi	13.0
Primary system efficiency, %	99.45
Primary system operating pressure, psia	550

Secondary Coolant System:

Turbine Generator:

Turbine throttle pressure, psia	2,414
Turbine throttle temperature, °F	1,000
Turbine throttle flow, lb/hr	6,485,300
HP turbine exhaust pressure, psia	644
IP turbine inlet pressure, psia	544
IP turbine inlet temperature, °F	1,000
IP turbine steam flow, lb/hr	6,445,200
LP turbine backpressure, in. Hg abs	2.00
Turbine arrangement	TC6F-31 in.

Regenerative Feedwater Cycle:

Feedwater flow, lb/hr	6,502,500
Final feedwater temperature, °F	330
Number of feedwater heaters	4

Helium Circulator Drive:

Motor speed rating, rpm	3,600
Motor-transformer efficiency, %	95
TG set power to motor drive, Mw(e)	48.2

Steam Generator:

Superheater outlet pressure, psia	2,515
Superheater outlet temperature, °F	1,005
Econ-evap, superheater Δp , psi	630
Reheater outlet pressure, psia	570
Reheater outlet temperature, °F	1,001
Reheater pressure drop, psi	45

Effect of Cycle Performance. Results of system analysis studies indicate that driving the helium circulators by electric motors instead of cold reheat steam results in the following changes:

1. The turbine-generator capacity must be increased by 49,400 kw. The incremental costs of the turbine and generator for this change are \$216,000 and \$266,000, respectively.
2. Current information from the turbine manufacturer indicates that there will be a \$53,000 reduction in price of the turbine-generator as a result of the omission of the circulator turbine between the HP exhaust and the reheater inlet.
3. The plant net heat rate is slightly better (by 12 Btu/kw-hr). The improved heat rate is partly the result of the combined effect of various efficiencies:
 - a. HP turbine isentropic efficiency 84%
 - b. Generator 98.3%
 - c. Auxiliary transformers 99%
 - d. Electric motors 96%
 - e. Helium compressor only, for the Base Case (turbine drive) 79%
 - f. Helium compressor only, for the case of electric motor drive 77%
 - g. The circulator turbine isentropic efficiency at plant rated load (including effect of by-pass flow and throttling valve loss) 74%

The product of items a, b, c, d, and f yields a combined efficiency (from steam to gas compression) of 60.3% for the electric-motor-drive case. A corresponding product of items e and g for the Base Case yields 58.5%. The effect of the 12-Btu improvement in heat rate is reflected in an improvement in plant capability of approximately 1.38 Mw. The cost differentials (i.e., reductions in cost over the Base Case) related to the Base Case are:

Incremental capability cost	\$83,000
Differential fuel cost per year	\$12,400
Capitalized value of differential fuel cost, based on 13.44% capital charge rate	\$92,000

Plant Auxiliary Electrical Distribution System. The use of the electric-motor drives requires certain design additions to the plant auxiliary electrical system. A relatively preliminary estimate of these with the associated cost extras is given in Table 1.20 using expected (nominal) ratings of switchgear and transformers. These ratings and related details will require updating when further details on the electric motor design are developed.

The design provides for a twin busbar system and two half-size unit auxiliary transformers, each supplying three circulator motors, to assure power supply to the circulators (through redundancy) in the event of an

Table 1.20
ELECTRICAL SYSTEM ADDITIONS

<u>Additions</u>	<u>Cost (\$)</u>
a. Two 25-Mva, 3-phase, unit auxiliary transformers, 20 kv/13.8 kv	143,000
b. One 50-Mva, 3-winding, 3-phase, reserve auxiliary transformer, with one 345-kv primary winding and two half-size secondary windings	161,000
c. Four sections of 15-kv, 3-phase switchgear, from transformer secondary to busbars, 2000-amp, 500-Mva interrupting rating	38,800
d. Two sections of 15-kv, 3-phase, bus tie switchgear, 1200-amp, 500-Mva interrupting rating	15,900
e. Six sections of 15-kv, 3-phase switchgear for six motors suitable for line reactor (reduced voltage) start, 1200-amp, 500-Mva interrupting rating	103,700
f. Six metal-enclosed, 3-phase, starting reactors for 10,000-hp, squirrel-cage induction motors, 15 kv	158,700
g. Twelve sets of 3-phase load-interrupter switch units, 6 amp, 15 kv	30,000
h. One set of 15-kv single-core interconnection cables, approximately 6000 ft	9,200
i. One 345-kv, 3-phase, 1600-amp, power circuit breaker for reserve auxiliary transformer primary, 25,000-Mva interrupting capacity	180,000
j. Two 3000-kw, 13.8-kv, standby diesel generators each for starting up to three circulator motors at half speed for pressurized primary system, and at full speed for depressurized condition. Generators and motors assumed started and brought up to speed together	390,000
k. Two standby diesel generator feeder circuit breakers, 15-kv, 3-phase, 1200-amp, 500-Mva interrupting rating	158,700
l. Installation cost for diesel generators, item j	46,800
m. Installation cost for transformers, items a and b	12,200
n. Installation cost for the remaining items, switchgear and cables	27,000
o. Allowance for cable penetrations into helium-filled motor compartments, installed	<u>30,000</u>
INSTALLED DIRECT COST OF THE ELECTRICAL SYSTEM	<u>\$1,505,000</u>

electrical fault, and to keep the short-circuit interrupting capacity of the circuit breakers within economic limits. The very high voltage level of the reserve auxiliary transformer primary (most probably something like 345 kv) makes two separate transformers uneconomic, and a single transformer with dual half-size secondaries is considered.

The large-capacity circulator motors will require a relatively high voltage (perhaps 13,800 v), whereas 4160 v is adequate for the rest of the station auxiliary power. Therefore, this preliminary estimate is based on a separate electrical system for the circulator motors.

At a detailed evaluation stage, possible integration of the circulator auxiliary electrical system with the remainder of the plant auxiliary system can be explored if interlocks and safeguards considerations make this attractive. Also, a pony motor for emergency operation will be compared with the alternative of a two-speed winding on the main motor, and the cost estimate will be updated for the scheme that appears most attractive.

As indicated in Table 1.20, the total cost of additions to the electrical system is currently estimated at \$1,505,000, but this is subject to revision as design details evolve.

Piping System. A major saving anticipated with the electric motor drives is due to substantial reduction in the run of cold reheat steam piping and this is summarized in Table 1.21 where cost items applicable to the determination of a preliminary cost differential are shown. Items such as safety and relief valves, non-return valves at the HP turbine exhaust, circulator auxiliary system piping, and instrumentation, which at this stage of design cannot be identified as being different between the two cases, are excluded from the comparison. The costs in Table 1.21 are installed costs including fittings, hangers, supports, and thermal insulation. The pipe runs generally correspond to those shown in Figs. 1.18 and 1.23, respectively.

It is not clear at this time what penetration flanges will be required for mounting the electric motor drives. Also, the detail design for the steam-driven circulator piping penetrations has not been established, though it is generally felt that these should be relatively simpler and cost less than the PSC circulator penetrations. Nevertheless, it is believed that there will be some cost saving for the electric drive case on this item, and this is judged to be approximately \$150,000 in favor of the electric drives.

Electric-motor Cooling Water System. It is expected that a closed cooling water system using deaerated, low-solids, controlled-pH water will be required for cooling the circulator electric motor drives. Total heat removal requirement for the six motors is estimated at approximately 8 million Btu/hr. The closed-water system can conceivably be integrated with the PCRV cooling water system. An installed cost allowance of \$35,000 has been made at this time for the system including the necessary pumps, heat exchangers, addition of capacity to the service water system, instrumentation, and controls.

Prenuclear Flow Tests. An outside electric power line of adequate capacity will normally be available for plant startup. The motor-driven circulators can therefore provide the necessary helium circulation for pre-nuclear primary flow tests without any supplementary equipment. If the circulators are steam powered,

Table 1.21
PRELIMINARY PIPING COST DIFFERENTIAL

	<u>Cost (\$)</u>
Cold Reheat Piping, Base Case (circulator turbines in the cold reheat line):	
Piping from HP turbine exhaust to circulator turbine	427,700
Piping from circulator turbine to reheater inlet	154,000
Allowance for auxiliary steam piping for startups and shutdowns	37,500
Block valves and check valves	257,000
Control valves	352,800
Differential cost of piping penetrations over the cost of penetrations for the electric motors	<u>150,000</u>
SUBTOTAL	1,379,000
Cold Reheat Piping, Case of Motor-driven Circulators:	
Piping from HP turbine exhaust to reheater inlet	325,000
Block valves and check valves	<u>105,000</u>
SUBTOTAL	430,000
COST SAVING (Comparison of 2 items above)	<u>949,000</u>

NOTE: Cold reheat system piping is carbon steel A-106-B. Only that part of piping relevant to the determination of the cost differential is included. Piping costs are installed costs including fittings, hangers, supports, and thermal insulation.

the steam for the prenuclear testing must be provided from other sources. This will not be a problem when a second unit is added at a given site after the first unit (whether nuclear or fossil fueled) is already operating. On some of the first large HTGR units, however, temporary package boiler capacity of the order of 700,000 to 800,000 lb/hr will have to be rented to provide the steam for the Base Case employing steam-driven circulators. It is difficult to estimate a definite cost for the renting of package boilers, since it depends considerably on the unit size, condition, timing, and location of availability. Extrapolating information gathered on PSC, a preliminary allowance of \$1,000,000 for steam provision for the prenuclear tests on the 1000-Mw Base Case is judged appropriate. This is based on running three of the steam generators at full flow with the reactor at 50% flow near rated pressure and temperature, which is considered adequate. With the electrically-driven circulators, of course, full reactor flow can be produced during the prenuclear tests.

Emergency Shutdown Power. As stated under Item j of Table 1.20, the present cost estimate allows for starting up to three electrically-driven circulators (in the event of loss of combined turbine trip and loss of outside power) from one of the standby diesel generators at half speed for the pressurized primary system, and at full speed for the depressurized condition. The diesel generator and motors are assumed to be brought up to speed together. At a detailed stage, the economics of doing this instead with a pony motor will be evaluated.

The cost of providing diesel generators can be reduced if they are considered part of the peaking plant (usually a gas-turbine generator package) being currently purchased by many utilities. (Approximate prices recently reported by Electrical World work out at \$77.50/kw for a 20-Mw gas turbine generator.

The auxiliary boiler capacity is linked to various accidents, and proper consideration of this requires a good deal of analysis that is still to be done. It is estimated, however, that the plant with the electric motor drives could do with a total auxiliary steam capacity that is approximately 60,000 lb/hr smaller than the Base Case. Based on the cost of the PSC boiler, a figure of approximately \$1.62/lb-hr is used for the differential boiler cost plus about 10% for installation.

Seal and Lube System. In the two types of drives considered, there is some difference between the seal system provision for the drive end of the compressor shaft. The type and location of seal and lube oil auxiliaries could also be different. The differences cannot be evaluated until the seal system details of the electric drive are established.

Operation. The major operational difference of the electric motor drive is that there is no possibility of circulator overspeed, since the induction motor is essentially a constant-speed machine. The cooling water system for the motor and the adjustable guide vanes necessary for the compressors driven by constant-speed motors will require design attention for reliability and surveillance. There appears to be no significant difference between the two control systems. In one case the turbine valves are positioned by an electric-hydraulic system; in the other, the guide vanes are positioned by a servomotor of some kind. Although the electric drives can receive power from more than

one outside power transmission line, the ultimate reliance in the event of a blackout will be on standby in-house diesel generators. The steam drives also depend on fossil-fuel (i.e., auxiliary) boilers. The operational availability of a motor of special design will need evaluation vis-a-vis that of single-stage steam turbines. Time periods required for any projected maintenance will be affected by the detailed steps required for disconnection of the motor from the circulator shaft and seal arrangement as against removal of the penetration piping and circulator in the Base Case. These aspects will need examination when additional design details become available.

Cost Summary and Conclusions. The various cost differentials discussed in the foregoing sections are summarized in Table 1.22. Items 10 and 11 of this table show the direct and total differential capital costs respectively as currently estimated. After the capitalized fuel cost saving and prenuclear test cost saving (items 12 and 13) are subtracted from this figure, the net penalty associated with the electric drives is nominally \$3,885,000 (item 14).

The estimated uncertainties in the various cost predictions presently are:

<u>Item</u>	<u>Costs, Including Indirects (\$)</u>
1	±850,000
4	±350,000
9	±270,000
13	±250,000
Total	±1,720,000

Thus, the evaluated total cost penalty may conceivably add up to something between \$2.2 and \$5.6 million.

The primary objective of this study was not to undertake a detailed cost analysis, but to obtain an early indication of the magnitude of the cost differential for using electric-motor-driven circulators and the probable degree of uncertainty in the present estimates. The study indicates that the uncertainties are sufficiently large to warrant invitation of bids from appropriate manufacturers for generation of detailed cost and design information for the electrically-driven circulators. Concurrently, Gulf General Atomic will supplement this with interrelated in-house effort on safeguards and shutdown philosophy, electrical system design, auxiliary steam system design, and better definition of the piping and prenuclear testing provisions.

The emphasis of the study will be on rotor dynamics as modified by the vertical configuration, the two-pole construction, and the small air gap typical of induction motors; adjustable guide vane design; the effect of helium environment; bearing seals and lubrication arrangement with appropriate selection of the lubrication fluid and the type and location of auxiliaries; electrical system design including standby motive power; the addition of a pony motor versus two-speed main motor; and the effect of all these factors on piping, piping penetrations, controls, instrumentations, reliability, and safeguards.

Table 1.22
COST DIFFERENTIAL FOR ELECTRIC-MOTOR-DRIVEN HELIUM CIRCULATORS
(Preliminary Costs)

Cost Additions

1. Cost differential of electric-motor-driven circulators over the steam turbine driven machines of the Base Case	\$2,700,000	
2. Incremental main turbine cost for increased rating	216,000	
3. Incremental generator cost for increased rating	266,000	
4. Incremental auxiliary electrical system cost	1,505,000	
5. Allowance for cooling water system for the electric motor	<u>35,000</u>	
Total Cost Additions		\$4,722,000

Cost Savings

6. Reduction in turbine generator cost for omitting circulator turbine	53,000	
7. Differential capability cost	83,000	
8. Reduction in auxiliary boiler cost, installed (3.7)	107,000	
9. Reduction in piping cost for cold reheat and auxiliary steam, installed, including effect of penetrations	<u>949,000</u>	
Total Cost Savings		<u>1,192,000</u>
10. Differential capital cost, direct		3,530,000
11. Differential capital cost, including 41% indirects		4,977,000

Additional Savings

12. Capitalized fuel cost savings	92,000	
13. Allowance for prenuclear tests	<u>1,000,000</u>	
Total Additional Savings		<u>1,092,000</u>
NET NOMINAL PENALTY		
Associated with Electric Drives ^a		<u>\$3,885,000</u>

^aIn text the cost uncertainties give this figure a possible spread of between \$2.2 and \$5.6 million.

Steam-to-steam Reheat Parameters

An analysis of a heat cycle using a steam-to-steam reheater was performed. The cycle studied is a variation of the Base Case cycle with steam-to-steam reheating in place of integral gas-to-steam reheating. A principal consideration for reheating is the moisture level in the last stages of the LP turbine. A moisture level comparable to the Base Case allows the selection of the reference turbine generator (TC6F-31 in.). Higher moisture levels require the use of the more expensive cross-compound turbine similar to the one selected for the no-reheat cycle. Reheating is accomplished using high-temperature steam from the main steam line (live steam) as the heating medium. The maximum amount of heat per pound of main steam that can be released to the reheat steam in a heat exchanger depends on the cold reheat steam temperature. If the initial temperature of the steam to be reheated is low enough, condensing of the live steam is permitted and a larger amount of energy may be released for a given flow.

The reheating process could be performed either before or after the IP turbine. If before, the steam would have to be reheated from approximately 650° to 900°F or higher in order to prevent excessive moisture in the last-stage blading (LSB). The live steam from the steam generator required by the steam-to-steam reheater would have to enter at 1000°F and leave at about 750°F in order to give a reasonable value of the log-mean-temperature difference (LMTD) for the heat exchanger. The live steam flow required for these conditions would be approximately 71% of turbine exhaust steam because of the non-condensing type of operation. The disadvantage of steam-to-steam reheating before the IP turbine is the large live-steam requirement, which tends to penalize both the steam generator (which must supply the live steam for the reheater) and the steam-to-steam reheater. Another disadvantage is the absence of a good thermodynamic way in which to make use of the high-pressure steam leaving the reheater. The turbine manufacturer does not permit readmission of such a large quantity of steam to an intermediate stage.

If, however, reheating is accomplished between the IP and the LP turbines, a steam temperature of only 690°F is required to yield a LSB moisture comparable to that of the Base Case heat cycle. The steam to be reheated has initial conditions of approximately 190 psia and 421°F. These conditions permit the live steam to be condensed efficiently in the reheat heat exchanger. For the heat cycle analysis, the 1000°F live steam was condensed and subcooled to 550°F, thus reducing the required steam flow to about 15.8% of the reheat steam flow. It may be concluded, therefore, that the live steam demand for low-pressure reheating is less than one-fourth the flow required when reheating at the intermediate pressure and appears to be the more economical choice for this type of reheating. An alternative to this type of reheating was discussed in a previous section.

The heat cycle parameters are shown in Table 1.23. Figures 1.24, 1.25, and 1.26 show the heat balance diagram, the primary circuit conditions, and the conditions in the steam-to-steam reheat, respectively.

Preliminary details and cost basis of a steam-steam reheater design approximately applicable to the Base Program steam-steam reheater were

Table 1.23
STEAM-TO-STEAM CYCLE PARAMETERS
(Case BP7)

Over-all Plant Performance:

Gross turbine generator output, Mw(e)	974.6
Net plant output, Mw(e)	956.2
Net plant efficiency, %	37.67
Net plant heat rate, Btu/kw-hr	9,059

Primary Coolant System:

Reactor gross thermal power, Mw(t)	2,538
He flow through circulators, lb/hr	8,862,000
He temperature at circulator inlet, °F	612
He temperature at steam generator inlet, °F	1,427
Primary system pressure drop, psi	13.0
Primary system efficiency, %	99.45
Primary system operating pressure, psia	550

Secondary Coolant System:

Turbine Generator:

Turbine throttle pressure, psia	2,414
Turbine throttle temperature, °F	1,000
Turbine throttle flow, lb/hr	6,764,300
HP turbine exhaust pressure, psia	767
LP turbine inlet pressure, psia	176
LP turbine inlet temperature, °F	690
LP turbine steam flow, lb/hr	6,414,900
LP turbine backpressure, in. Hg abs.	2.00
Turbine arrangement	TC6F-31 in.

Regenerative Feedwater Cycle:

Feedwater flow, lb/hr	7,792,600
Final feedwater temperature, °F	361
Number of feedwater heaters	4

Helium Circulator Turbine Drive:

Inlet pressure, psia	744
Inlet temperature, °F	701
Outlet pressure, psia	579
Approx. power margin via bypass flow, %	7

Steam Generator:

Superheater outlet pressure, psia	2,515
Superheater outlet temperature, °F	1,005
Economizer-evaporator, superheater Δp , psi	630

Steam-to-steam Reheater:

Reheater outlet pressure, psia	180
Reheater outlet temperature, °F	690
Reheater pressure drop, psi	7

Fig. 1.24--Steam-to-steam reheat cycle BP7, 2538 Mw(t)

Fig. 1.25--Primary circuit parameters, case BP7

Fig. 1.26--Steam-to-steam reheater design conditions

obtained from General Electric. Although there are some differences in the accessories included relative to the Base Program reheater, this information is considered more reliable than parallel data from another vendor, since GE has had experience with this equipment in the boiling water reactor plants, and further, will integrate the main turbine IP-LP crossover connections with the steam-steam reheater, which is in series with IP and LP turbines. Additional details of this study will be discussed in a subsequent report.

Heat Cycle Summary

A summary comparison of heat rates obtainable in the various cycles so far studied is presented in Table 1.24 for advance information. The significance of these heat rates in the context of over-all evaluation of the alternatives will be discussed in future presentations.

Table 1.24
PRELIMINARY CYCLE PERFORMANCE

Base Case:

Input	2538 Mw(t)
Output	1000 Mw(e) net
Heat rate	8663 Btu/kw-hr

Heat-rate Diff. over Base Case

Other Alternatives:

Reactor-only PCR/V	0.4% worse
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Reheat Options:

Nonreheat	4.2% worse
Steam-steam reheat	4.6% worse

Circulator Drives:

Condensing turbine	0.3% better
Electric motor	0.15% better
High-press., high-temp. turbine after the final superheater	1.9% better
High-press. turbine between the pre- superheater and the final superheater	2.8% better
Noncondensing turbine in parallel with the reheater and IP turbine	1.9% worse

Reactor Shutdown Heat Removal

A study of reactor shutdown heat removal was initiated. An analytical model of the reactor core has been prepared and tested using a previously developed computer program. The results are being examined before making further runs. The cooling ability of one or more steam generators operating under shutdown heat removal conditions is also being investigated. Both steady-state and transient steam generator models for the Base Case steam generator are being prepared.

PCRV DESIGN

PCRV Stress Analysis

Analysis was completed on the computer work of the finite-element analysis of a typical multicavity PCRV horizontal cross section. Two different analyses were performed.

1. Time-independent analysis for the loading condition "prestressing plus steady-state temperature."
2. Time-independent analysis for the loading condition "prestressing plus steady-state temperature plus 650-psi internal pressure." In order to be able to observe the concrete cracking propagation, the pressure was applied in increments of 130 psi.

The results of the study are being evaluated.

Vessel Design

A PCRV with 1000-ton linear tendons resembling the technology developed for the Fort St. Vrain PCRV was sized, and the amount and distribution of the prestress determined. Twenty percent excess prestress above the maximum pressure will be provided in order to eliminate the need for large amounts of reinforcement bars. The design of the PCRV follows the proposed design criteria. Drawings were prepared for a PCRV with 3000-ton linear tendons, combining the existing technology of the 1000-ton linear tendon system with some of the advantages of wire-winding schemes. Drawings are in process for a PCRV with helical tendon arrangement. Layouts of wire-wound PCRV's with various steam generator cavity closures were prepared. Drawings for the following cases were made:

1. Prestressed concrete plug closure externally supported by the PCRV.
2. Prestressed concrete closure embedded in the PCRV.
3. Steel closures.
4. Concrete-box closure.

PCRV Support

Integrated PCRV support schemes were studied. No satisfactory support scheme was found. High seismic forces, space limitations, strain incompatibilities, and obscure PCRV support structural interaction make such designs undesirable if not infeasible.

A PCRV support scheme was designed which, from seismic response considerations, is equivalent to a rigid support. Such a scheme is limited however to maximum horizontal ground accelerations of about 0.2 g rather than the specified 0.25 g. An amplification of approximately 3.5 times of the ground motion acceleration for the PCRV and its internals has to be accounted for in the design.

A flexible PCRV support scheme using elastomeric bearing pads was designed in conjunction with vertically prestressed concrete support columns. This scheme produces a natural period of vibration of the PCRV of about 2 sec,

resulting in lower PCRV accelerations than the ground accelerations. The associated relatively small forces can be quite easily resisted. Lateral deflections of about 4 in. for the operational earthquake and of about 6 in. for the design-basis earthquake must be accounted for in the design of piping and other connections to the PCRV.

PCRV Internals Design

A study on the PCRV penetration closure design requirements for the case of full secondary containment has been completed and the results are being reviewed. A system description for penetration liners and closures has been completed in preliminary form and is also being reviewed. Sizing calculations for the 12-ft-long graphite core support posts indicate that they must be at least 8-1/2 in. in diameter. Drawings were initiated on the designs of the Base Case top head penetrations, helium purification wells, and safety relief valve penetration and well.

A dynamic analysis of the core excitation under seismic excitation indicates that a core restraint system consisting of 7-ft-long I beams, approximately 8 in. WF, simply supported, spaced at 10° intervals around the circumference is acceptable. This conclusion is based on assumed core accelerations of 0.2 and 0.6 g with distances of travel of 1/4 in. and 3 in.

Radiation Analysis

Calculations were performed that determined that the core - side-reflector layout, which resulted in a liner inside diameter of 40 ft 3 in., is satisfactory. This conclusion was based on evaluations of (1) fast-neutron exposure of the PCRV liner, (2) gamma exposure of the innermost tendons, and (3) nuclear heating of the PCRV. The maximum 40-year fast-neutron exposure of the liner is less than the acceptable limit of 2×10^{18} nvt. The maximum transient nuclear heating rate in the PCRV is equivalent to an incoming heat flux of less than 140 Btu/ft²-hr over a few-square-foot area, causing a local 70°F temperature rise about 1 ft into the concrete.

Calculation of Minimum Side Reflector Thickness. Minimum side reflector thickness was determined by a weighting function method in which distances from an appropriate dose point on the liner to individual fuel surfaces are averaged by use of the weighting function $F(\pi/2, b_1)$. Study of the fuel region drawing indicated that a dose point opposite fuel regions 63, 68, 73, 83, or 88 would "see" the minimum reflector thickness. This dimension turned out to be 42 in. for the case of a 5-1/8-in. thermal barrier, or 40 in. for the case of a 7-1/8-in. thermal barrier.

Determination of Local Power Density. The pertinent characteristics of the 1000-Mw(e) Base Program core are:

1. Number of fuel columns 601
2. Number of blocks per column 6
3. Average power density (w/cm³) 7.9

A study of the time-averaged relative power in the above-mentioned regions yielded the data in Table 1.25.

Table 1.25
TIME-AVERAGED POWER

Time	Region 63	Region 68	Region 73	Region 78	Region 83	Region 88
End of 4 years	0.53	0.70	0.845	0.53	0.70	0.845
End of 5 years	1.20	0.50	0.63	1.20	0.50	0.63
End of 6 years	0.70	1.144	0.48	0.70	1.144	0.48
End of 7 years	<u>0.57</u>	<u>0.68</u>	<u>1.12</u>	<u>0.57</u>	<u>0.68</u>	<u>1.12</u>
Average	0.75	0.76	0.77	0.75	0.76	0.77

A radial relative power of 0.77 was used for time-integrated exposure calculations. For transient heating calculations the peak value of 1.20 was selected.

An axial power profile of the core is shown in Fig. 1.27. Over the 3-ft increment of height from $Z/L = 0.3$ to 0.5 the value of $P/\bar{P}$ is approximately 1.4. This value was used for both dose and heating calculations. The resulting time-averaged local power density is $7.9 \times 0.77 \times 1.4 = 8.5 \text{ w/cm}^3$.

Fast-neutron Flux. The $E > 1 \text{ Mev}$ neutron flux was obtained from previous calculations made at Gulf General Atomic. Using these data and the characteristics of the 1000-Mw(e) Base Program core listed in Table 1.26, Fig. 1.28 was prepared and shows the resulting $E > 1 \text{ Mev}$ flux plotted for a 5-1/8-in.-thick thermal barrier.

Table 1.26
CORE CHARACTERISTICS

Average core power density	7.9 w/cm ³
Axial peaking factor (over height increment of 3 ft)	1.4
Radial power distribution	0.77 in outer foot of core
Minimum side reflector thickness, as determined for shielding purposes	42 in.
Graphite densities:	
Core	1.26 g/cm ³
Reflector	1.60 g/cm ³
Thickness of borated portion of reflector	9 in.
Clad thickness	1/8 in.
Insulation thickness	5 in.
Liner thickness	3/4 in.

Fig. 1.27--Axial profile for 1000-Mw(e) Base Program core

Fig. 1.28--Maximum fast-neutron flux ($E > 1$ MeV) in side regions of 1000-Mw(e) HTGR; thermal barrier thickness = 5-1/8 in., slab geometry

The maximum fast flux on the inside surface of the liner is approximately 2.8×10^8 n/cm²-sec. A plot made for the case of a 7-1/8-in. thermal barrier, Fig. 1.29, shows that the flux increases to 4.2×10^8 n/cm²-sec owing to the 2-in. reduction in graphite thickness.

Calculation of Liner Exposures. The PCRV liner exposure is determined as follows for the 5-1/8-in. thermal barrier:

E>1 Mev flux		Factor for E<1 Mev flux		Uncert. factor	
↓		↓		↓	
2.8×10^8	x	1.5	x	2.0	x
Load factor		Sec/yr		Years operation	
↓		↓		↓	
0.85	x	3.16×10^7	x	40 = 1.1×10^{18} nvt .	

In the case of the 7-1/8 in. thermal barrier, the liner exposure is 1.6×10^{18} nvt.

Both the above values are acceptable for a liner material fabricated from ASTM A537 Grade B as is being supplied for the Fort St. Vrain HTGR. The exposure should not exceed approximately 2×10^{18} nvt.

Since the calculated exposure is approaching the permissible value, the side reflector thickness for the 1000-Mw(e) Base Program HTGR cannot be reduced.

Estimate of Gamma Exposure of Inner Tendons. Figure 1.30 shows the gamma dose rate in the side regions around the core and includes corrections for local core power density and reflector thickness to provide the dose rate at the inner tendons, assumed to be 2 ft from the liner. The gamma dose rate 2 ft into the PCRV is 400 rad/hr.

Because of inherent uncertainties in the calculation of capture gamma production rates and buildup of very low energy gammas in graphite, a somewhat larger uncertainty factor must be incorporated, at least on the downside; a value of +2.0, -4.0 is used. Calculation of the exposure to the inner tendons proceeds as follows:

Gamma dose rate		Uncert. factor		Load factor	
↓		↓		↓	
$400 \frac{\text{rad}}{\text{hr}}$	x	2.0	x	0.85	x
Hr/year		Years Operation			
↓		↓			
8750	x	40	=	2.4×10^8 rad .

Fig. 1.29--Maximum fast-neutron flux ($E > 1$ MeV) in side regions of 1000-Mw(e) HTGR; thermal barrier thickness = 7-1/8 in., slab geometry

Fig. 1.30--Maximum operating gamma dose rate in side regions of 1000-Mw(e) HTGR; thermal barrier thickness = 5-1/8 in., corrected for geometry

The lubricant used between the tendons and the concrete will be able to withstand this exposure without excessive degradation of properties or generation of hydrogen gas.

Gamma exposure with a 7-1/8-in.-thick thermal barrier would be about 30% higher.

Calculation of Nuclear Heating Rates. Nuclear heating rates in the side regions of the Base Program 1000-Mw(e) core were determined from extrapolation of work done at Gulf General Atomic on nuclear heating in other large cores. The local transient power density was calculated to be 13.3 w/cm³.

The total nuclear heating rates are shown in Fig. 1.31. This plot describes the maximum transient heating rate over a local cross-sectional area of a few square feet; the curves cannot be used directly to calculate total reflector heating.

The uncertainty in the PCRV nuclear heating rate is estimated to be the same as that for the gamma dose rate, namely, +2.0, -4.0. Equivalent nuclear heat flux into the PCRV can then be found from

$$Q_{eq} = 2 \int_0^{\infty} C q_0 e^{-x/\lambda} dx ,$$

where Q_{eq} = equivalent heat flux in Btu/ft²-hr,

q_0 = initial volumetric heating rate in mw/cm³,

C = conversion factor = 97 (Btu/ft²-hr)/(mw/cm³),

x = distance from PCRV in feet, and

λ = e-folding distance of volumetric heat rate in feet.

For $q_0 = 1.6$ mw/cm³ and $\lambda = 0.46$ ft, $Q_{eq} \approx 140$ Btu/ft²-hr. This heat flux would cause a local temperature rise of about 70°F, occurring about 1 ft in from the liner.

Relationship of Circulator and PCRV Internals Design

An investigation was made of various circulator designs, requirements, and arrangements, and the principal effects on the PCRV liner, cooling system, and thermal barrier design were evaluated. The following designs and options were considered:

1. Helium Circulator Drive Options
 - a. Electric-motor-driven circulators.
 - b. Steam-turbine-driven circulators.
2. Plant Cycle Options
 - a. Steam-steam reheat steam turbine.
 - b. No-reheat steam turbine.
 - c. Superheat steam turbine.
 - d. Extraction steam turbine.

Fig. 1.31--Maximum transient nuclear heating rates in side regions of 1000-Mw(e) HTGR, slab geometry

3. Plant Arrangement Options

- a. Base Case with separate secondary containment.
- b. Reactor-only PCRIV.

Work is continuing on the investigation and evaluation of insulation concepts, types, and materials to determine the thermal barrier design to be adopted for the advanced HTGR reactor design. Work was begun on a system description and conceptual design of the thermal barrier insulation system for the advanced HTGR reactor design based on preliminary information developed from the studies.

PCRIV Liner, Coolant System, and Thermal Barrier

The following drawings for the Base Case have been completed in preliminary form:

1. Bottom head thermal barrier installation.
2. PCRIV liner barrel section.
3. Thermal barrier general arrangement.
4. PCRIV liner bottom head.

Work is continuing on the investigation and evaluation of insulation concepts, types, and materials to determine the thermal barrier design to be adopted for the advanced HTGR reactor design, and a system description and conceptual design is being prepared.

Work has begun on a system description and conceptual design of the liner and cooling system for the advanced HTGR reactor design.

PCRIV Internals, Penetrations, and Closures

The system description for reactor internals and structures has been revised to incorporate review comments.

Drawings for the Base Case refueling penetrations and helium purification train equipment have been completed in preliminary form and are being reviewed. Additional drawings showing alternative design concepts for the steam generator cavity closure are being prepared for the purpose of obtaining comparative cost estimates for various closure concepts.

Core support post sizes for the Base Case without reheat (plenum height of 7 ft 3 in.) and reactor-only PCRIV (plenum height of 6 ft 0 in.), have been calculated to be 7 in. and 6.4 in., respectively.

An investigation of the variations in internals, structures, and penetrations with various Base Case steam generator design-reheat options is nearing completion.

A preliminary analysis has shown that the steam generators of the side outlet reactor-only PCRIV can be supported in cantilever fashion from the helium duct leading from the PCRIV to the steam generator. Guides to restrain lateral movements at the top of the steam generator and perhaps also at the bottom would be utilized. This scheme eliminates a separate steam generator support structure.

STRUCTURES

Arrangements have been made with United Engineers and Constructors, Philadelphia, Pennsylvania, to perform a study to determine the structural feasibility of the Base Case reactor building and of the secondary containment. The initial phases of the work are described below.

Reactor Building Exterior

Three types of construction are being investigated for the reactor building exterior: (1) reinforced concrete, (2) prestressed precast concrete, and (3) structural steel with metal wall siding. The major emphasis of the design effort for the reactor building was under tornado and seismic loading. The analyses were performed in accordance with the parameters given below.

Two tornado loading conditions are considered. For a wind velocity of 300 mph in a storm traveling 60 mph, the structure may experience a total wind velocity of 360 mph. ASCE Paper 3269 translates this velocity into a pressure of 332 lb/ft². For a structure having the geometry of the reactor building, the distribution of the wind pressure will result in the following loadings:

Windward side	0.9 (332)	External
Leeward side	0.5 (332)	Suction
Side walls	0.8 (332)	Suction
Roof	0.6 (332)	Suction

These loads are proportioned per ASCE 3269 and do not take into account pressure redistribution from the vent system. Final analyses could require some modification of these loadings.

The second loading considered is a static pressure drop of 3 psi assumed to occur during passage of the tornado. Because of the venting system, this pressure acts on the structure as an external load. Final tornado pressure transient analysis could modify the 3 psi value.

The design missile is a fir plank 4 in. by 12 in. by 12 ft long, weighing 105 lb and traveling at 200 mph. As design details are completed, a check will be made to ensure that the various structural elements have sufficient ductility to absorb the kinetic energy of the missile.

The loads on the structures caused by earthquakes are determined by dynamic analysis. The structure is idealized into a series of lumped masses and weightless elastic springs. The analysis is performed in two stages: the determination of the natural frequencies of the structure and its mode shapes and the response of these modes to the earthquake by the spectrum-response method.

The criteria are as specified in DC-70 (see Appendix B of the previous quarterly report, GA-9227). The computer program used is the Structural Analysis and Matrix Interpretive System (SAMIS) developed by Philco-Ford for the Jet Propulsion Laboratories. This program has been modified by United Engineers and Constructors to incorporate the response spectra contained in DC-70.

The natural frequencies and mode shapes are computed from the equations of motion of the lumped masses. The basic dynamic equation (in matrix form) is

$$\lambda^2 [M](\delta^*) = [K](\delta^*) \quad ,$$

where λ^2 = desired eigenvalues,

(δ^*) = desired eigenvectors (mode shapes),

$[M]$ = total mass matrix, and

$[K]$ = total stiffness matrix.

Preliminary calculations for SAMIS input requirements were:

1. Establish a coordinate system and node points.
2. Determine geometric properties of the model that most accurately describe the building.

The SAMIS (BILD) link internally generates the stiffness and mass matrices in accordance with the geometric input properties.

1. The first step in the solution of the dynamic equation is the Choleski decomposition and inversion of the mass matrix: (SAMIS CHIN link).

M decomposed = $[M^U]$, and $[M^U]^{-1} = [M^U]$ inverted

$$[M^U] = U \quad ,$$

$$[M^U]^{-1} = U^{-1} \quad .$$

2. Transpose $[M^U] = U$: (SAMIS FLIP link),

$$U \text{ transposed} = U^T \quad .$$

3. The Choleski decomposition and inversion of $[K]$ to form $[K^{-1}]$ (SAMIS CHOL link) forms product $[K^{-1}]U^T$.

- a. The original dynamic equation is

$$\lambda^2 [M](\delta^*) = [K](\delta^*) \quad .$$

- b. $[M] = U^T U$.

Substitute b into a.

- c. $\lambda^2 U^T U(\delta^*) = [K](\delta^*)$.

Define:

$$(\bar{\delta}) = U(\delta^*) \text{ and premultiply both sides by } U^{-1}.$$

- d. Therefore, $(\delta^*) = U^{-1}(\bar{\delta})$.

Substitute d into c,

$$\lambda^2 U^T U U^{-1}(\bar{\delta}) = [K] U^{-1}(\bar{\delta}) \quad ,$$

$$U U^{-1} = [I] = \text{identity matrix} = [M^U] [M^U]^{-1} \quad .$$

e. Therefore, $\lambda^2 U^T(\bar{\delta}) = [K] U^{-1}(\bar{\delta}) \quad .$

Premultiply e by $U[K^{-1}] \quad ,$

$$\lambda^2 U^T U [K^{-1}] (\bar{\delta}) = U [K^{-1}] [K] U^{-1}(\bar{\delta}) \quad ,$$

$$U [K^{-1}] [K] U^{-1} = [I] = \text{identity matrix} \quad .$$

f. Therefore, $\lambda^2 U [K^{-1}] U^T(\bar{\delta}) = (\bar{\delta}) \quad .$

$[K^{-1}] U^T$ is the result of step 3.

4. The product matrix $U [K^{-1}] U^T$ is solved for the characteristic roots λ^2 and vectors $(\bar{\delta})$ using the Jacobi method (SAMIS ROOT link). The frequencies are obtained from the roots of the above matrix; eigenvalues = $1/\lambda^2 = 1/W^2$. The frequency cps = $W/2\pi$.

The response of each mode of vibration to the earthquake ground motion is computed by the response-spectrum technique as follows:

1. The participation of each mode, P_n is computed from:

$$P_n = \frac{\sum_{M=1}^N (\delta_{mn}^{*x}) [M_m]}{\sum_{M=1}^N (\delta_{mn}^{*2}) [M_m]} \quad ,$$

where (δ_{mn}^{*x}) is the deflection of mass point m in mode n, and (δ_{mn}^{*x}) is the component of (δ_{mn}^{*}) in the direction of the earthquake.

2. The relative deflection of each mass is determined from

$$Y_{mn} = (\delta_{mn}^{*}) P_n \frac{S_{an}}{W_n^2} \quad ,$$

where S_{an} is the spectral acceleration of a single degree of freedom system with a frequency W_n and damping coefficient a.

3. For each mode, the shear forces and moments are calculated from the deflected shape from

$$[F] = [K] [Y_{mn}] \quad .$$

These are maximum values of the response in each mode. The total response is computed as the square root of the sum of the squares of the individual modes.

Building leakage during the design basis PCRV blowdown accident was evaluated. The criterion selected was 150% of building volume per day at a design pressure of 30 in. W.G. The basis of the evaluation is "Conventional Buildings for Reactor Containment," NAA-SR-10100.

Reinforced-concrete Reactor Building. Preliminary tornado wind structural analysis was performed for the reinforced-concrete reactor building assuming a uniform wall thickness applicable to slip-form construction and a series of planar rigid frame bents. Minimal bracing was assumed, and there were no ties either to the fuel handling structure or to the PCRV. Because of the severity of the loading, these structural forms proved inadequate for both stress and deflection. To fully appreciate the magnitude of the wind loadings, the wall thickness assumed for the slip-form model, and found to be inadequate, was 5-ft thick.

From the preliminary analysis it was concluded that a structure comprised of rigid frames, tied together by large horizontal girders with interior diaphragms was required. Columns 3 ft by 4 ft are spaced along the "L" line and are tied to the "P" line columns by horizontal struts located at elevations 208 ft 0 in., 172 ft 10 in., and 136 ft 11 in. In addition, diaphragms are located in the corners of the PCRV (but not tied to the PCRV) at the same elevations. The 5-ft clearance for rewinding the PCRV has been maintained, and only one opening has been supplied for removing and replacing the steam generators.

A static analysis of the reactor building structure has been done using a rigid frame computer program developed by United Engineers and Constructors. The structure model is a three-dimensional space frame with a series of beams and columns. Figure 1.32 shows the model used.

The operation of the program which uses the stiffness matrix method, are as follows:

1. Input data consist of member properties, loads, internal and external joint restraints, joint numbers, and joint coordinates.
2. Stiffness and load matrices are formed for each member.
3. The member stiffness matrices are added together to form the total stiffness matrix. A total load matrix is formed in the same manner.
4. A series of simultaneous equations is generated, having the form

$$[K]\Delta = \{P\} \quad ,$$

where $[K]$ = total stiffness matrix,

Δ = unknown deflections, and

$\{P\}$ = total load matrix.

5. This series is solved for deflections by using the Choleski triangular decomposition technique, which results in

$$\Delta = [K^{-1}] \{P\} \quad ,$$

where $[K^{-1}]$ is the inverse of the total stiffness matrix.

Fig. 1.32--Analytical model used for reinforced-concrete reactor building

6. The deflections are converted to terminal forces on members.
7. Final sizing of members and reinforcing is determined from terminal forces. Using this program, the reactor building is analyzed for the following load conditions:
 - a. Dead load.
 - b. Tornado wind loadings with wind from north, south, east, and west directions with the distribution discussed above plus dead load.
 - c. External load of 3 psi plus dead load.
 - d. Suction load of 1 psi plus dead load.

From the results of the analysis, three building schemes were devised:

1. Building as modeled.
2. Elimination of columns with equivalent uniform wall thickness between horizontal girders.
3. Elimination of columns and horizontal girders with equivalent uniform wall thickness.

Scheme 2 was found to have the disadvantage of both and was therefore eliminated. A comparison of schemes 1 and 3 showed that scheme 3 required about twice as much concrete as scheme 1, whereas scheme 1 required almost three times as much forming as scheme 3. Scheme 3 could be a continuous operation utilizing slip-form construction, whereas scheme 1 would have to be staged. Scheme 3 required approximately 25% more reinforcing than scheme 1. As a result of these considerations, scheme 3 was felt to be advantageous for both cost and scheduling.

However, with the increased mass for scheme 3, seismic loading and not tornado loading would control the design. Reinforcing requirements for scheme 3 could be as much as 60% greater than scheme 1, and this made scheme 1 the more economical of the two. Scheme 1 was therefore selected for this study.

Design of the reinforced concrete structural elements was performed in accordance with the ultimate strength design concept of the ACI Standard Building Code Requirements for Reinforced Concrete, ACI-318-63, Part IV-B, modified for no-load factors nor capacity reduction factors.

Tornado missile analysis for the reinforced concrete case has utilized the modified Petry formula. Conservatively, no credit has been taken for the energy absorbed by the deformation of the fir plank as it strikes the concrete.

Maximum penetration of the design missile into concrete has been determined, and based on a wall thickness of at least three times the calculated penetration, a minimum wall thickness of 12 in. has been selected.

The structure is presently being modeled for the seismic analysis.

The free volume of the reactor building has been determined as being $3.7 \times 10^6 \text{ ft}^3$. Allowing for 150%/day leakage yields $5.55 \times 10^6 \text{ ft}^3/\text{day}$. It has been assumed that, with proper treatment of construction joints, doors, etc., the major leakage would be through the concrete, recognizing that sufficient margin should exist to allow for other leak paths. The assumed design pressure is 30 in. W.G. The calculated leakage is far below the allowable.

Because of the large bearing stresses encountered under the PCRV and the fuel storage vault, it is necessary to carry these structures to bedrock. Therefore, to avoid differential settlement, the building foundations have been isolated. An evaluation of poured-in-place concrete versus slip-forming the same geometric-shaped building is under way.

A construction sequence and schedule is now being developed, and the cost analysis is essentially complete, except for a final review when the construction sequence and schedule are available. The design details for the concept are shown in Figs. 1.33, 1.34, and 1.35.

Prestressed Precast Concrete Reactor Building. As a result of the preliminary analysis of the reinforced concrete reactor building, it was concluded that a structural system similar to that of reinforced concrete would be required.

This system utilizes precast concrete plank siding framing into precast concrete columns spaced about 15 ft on centers. The columns frame into horizontal girders that are continuous around the structure in the form of a box. The girders are spaced about 30 ft on centers. Lateral loads are transmitted from the girders through vertical bracing to the ground.

The principal wind bents from grade up to elevation 208 ft are along Column lines D, L 6, and 10. Above elevation 208 ft, the wind bent moves from 10 line to 12 line to provide a clear area for crane operation.

Because of the requirement of maintaining a 5-ft clearance around the PCRV, it was necessary to increase the inside dimension of the building approximately 10 ft in the east-west direction.

As the plant design is finalized, modification of wind bracing along 10 line and L line may be required to clear major piping, etc.

The precast plank siding is commercially available from a number of manufacturers. Other members require special forms and would probably be cast on site, which would eliminate shipping problems for the large members. Member sizes are:

- 8-in. plank siding 40 to 48 in. wide.
- 28-in. x 60-in. columns.
- 33-in. x 13-ft horizontal girders.
- 28-in. x 40-in. bracing.

Lateral tornado loads are carried into the columns by the horizontal spans of planking. The columns transmit these loads into the horizontal girders. The girders act as boxed units that carry the loads to the side of the building where the loads are transmitted to the bracing and thence to the ground.

Fig. 1.33--Base Case reactor building arrangement with reinforced concrete structure; isometric and plan view at refueling floor

Fig. 1.35--Base Case reactor building arrangement with reinforced concrete structure; foundation plan

Structural design was performed in accordance with the ultimate strength design concept of the ACI Standard Building Code Requirements for Reinforced Concrete, ACI-318-63, Part IV-B, Chapter 26, modified for no-load factors and $f_{su} = 80\%$ of the minimum ultimate tensile strength of the tendons.

Drawings are being reviewed by a prestressed concrete equipment manufacturer for optimizing prestressing systems and achieving a most economic design. The 8-in. thickness for the precast planking has been found to be adequate for the tornado missile loading. The analysis was essentially the same as the analysis performed for the reinforced concrete concept.

For the precast concrete building, the principal leakage was assumed to occur at the precast element joints. With proper caulking, the building can sustain an internal pressure of 30 in. H_2O with leakage maintained well below the 150%/day.

As in the case of the reinforced concrete building, foundations are isolated from the PCRV and fuel storage vault foundations.

A construction sequence and schedule are now being developed and finalization of cost analysis will be performed.

A construction sequence and schedule is now being developed and finalization of cost analysis will be performed upon receipt of prestressed concrete equipment manufacturer's comments and completion of construction sequence and schedule. The design details for the concept are shown in Figs. 1.36, 1.37, and 1.38.

Structural Steel with Wall Siding Reactor Building. The design of the reactor building having structural steel with wall siding is essentially complete. The siding is precast planking below the refueling floor and metal blowout panels above the refueling floor.

A cost analysis has been initiated and the construction sequence and schedule are being developed.

Containment Structure

Three types of construction were investigated for the secondary containment structure: (1) reinforced concrete with thin steel liner, (2) prestressed concrete with thin steel liner, and (3) steel containment with separate concrete biological and missile shield. Concrete containment structures are inherently capable of withstanding tornado wind loadings and tornado missiles. In each design concept, the structure will be checked for adequacy to resist tornado effects, but the tornado is not expected to control the design. The analyses were performed in accordance with the parameters described below.

Two tornado loading conditions are considered. For a tornado having a wind velocity of 300 mph and traveling at 60 mph, the structure may experience a total wind velocity of 360 mph. ASCE Paper 3269 translates this velocity into a pressure of 332 lb/ft². Taking into account the shape of the structure and the smoothness of the surface, this velocity pressure gives a loading of 150 lb/ft² on the projected area of the structure for the reinforced concrete

containment and the shield wall for the steel containment. Because the buttresses for the prestressed containment cause greater drag, the loading on this structure is 232 lb/ft^2 on the projected area of the structure.

The second loading considered is the static pressure drop of 3 psi assumed to occur while the tornado is passing. This pressure differential acts as a suction load on the exterior of the structure.

The design missile is the same as that used in the reactor building structural analysis.

The seismic analysis is performed by the same dynamic analysis method as is used for the reactor building.

The gross volume of the containment is $3.0 \times 10^6 \text{ ft}^3$. The volume of the interior structure is about $0.86 \times 10^6 \text{ ft}^3$, which yields a containment free volume of $2.14 \times 10^6 \text{ ft}^3$.

From the Gulf General Atomic specification, the corresponding pressure and temperature are 41 psia and 535°F . Gulf General Atomic is currently investigating the effects of heat sinks, etc., to modify the temperature at the inside surface of the containment. Using an atmospheric pressure of 14.7 psi yields a containment design pressure of 26.3 psig.

Reinforced Concrete Containment. The reinforced concrete containment is a reinforced concrete, steel-lined structure having a 4-ft 0-in.-thick cylindrical wall, 2-ft 6-in.-thick hemispherical dome, and a 7-ft 0-in.-thick base mat. The bottom liner plate is $1/4$ in. thick, the side wall plate is $3/8$ in., except for the first three courses, which are $1/2$ in. The dome thickness is $1/2$ in. These dimensions are primarily dictated by construction requirements. Design drawings are shown in Figs. 1.39 and 1.40.

The tornado wind structural analysis has been completed and is currently being reviewed.

For the seismic analysis, the containment structure has been modeled as a cantilever beam, founded on a rigid base with seven mass points, as shown in Fig. 1.41. Rocking has not been considered, since the structure is founded on competent rock. The effects of backfill have not been considered, though in a final design its effect on the structure should be evaluated.

Damping values have been selected as 5% for the operating earthquake and 7% for the maximum earthquake. Shear deformation and rotary effects are taken into account.

Each mass point is given three degrees of freedom, i.e., one lateral translation, one vertical translation, and a rotation about a horizontal axis. The first ten frequencies have been determined as:

PLAN OF REFUELING FLOOR EL 208'9
TOP OF PRECAST CONCRETE FRAMING -2'0"
(FLOOR FRAMING NOT SHOWN)

NOTE:
CONCRETE SPECIFICATION
PRECAST CONCRETE SHALL DEVELOP 5000 P.S.I. IN 28 DAYS
CAST IN PLACE (C.I.P.) CONCRETE SHALL DEVELOP 5000 P.S.I. IN 28 DAYS
REINFORCING STEEL
REINFORCING SHALL BE 60,000 P.S.I. MINIMUM YIELD STRENGTH BILLET STEEL
POST-TENSION TENDONS
INLAND-RYERSON "BBRV" POST TENSIONING SYSTEM (OR EQUIVALENT) SEE TABLE I & II FOR SIZES
WIRE IS .025 INCHES NOMINAL DIA.
ULT. STRENGTH = 240,000 P.S.I. GROUT AFTER TENSIONING.

Fig. 1.36--Base Case reactor building arrangement with precast concrete structure; isometric view and plan view of refueling floor

Fig. 1.37--Base Case reactor building arrangement with precast concrete structure; detail views

TABLE II - POST-TENSIONING WIRES									
COL. NOS.	D-6	E-6	D-10	D-12	D-12	D-12	D-12	D-12	D-12
TO OF GIR.	L-6	K-6	L-10	L-12	L-12	L-12	L-12	L-12	L-12
EL. 296'-9"	0'-0"	E-12	K-12	L-12	L-12	L-12	L-12	L-12	L-12
FUEL HANDLING LEVEL EL. 208'-9"	288 WIRES - 2714"	144 WIRES - 1357"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"
GRADE EL. 100'-0"	288 WIRES - 2714"	144 WIRES - 1357"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"
BOTT. FTG. EL. 79'-0"	288 WIRES - 2714"	144 WIRES - 1357"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"
BED ROCK EL. 50'-0"	288 WIRES - 2714"	144 WIRES - 1357"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"	288 WIRES - 2714"
DEPTH OF ANCHORAGE	22'-0"	22'-0"	22'-0"	22'-0"	22'-0"	22'-0"	22'-0"	22'-0"	22'-0"

Fig. 1.38—Base Case reactor building arrangement with precast concrete structure; foundation plan

SECTION A-A
1/8" = 1'-0"

Fig. 1.39—Reactor building alternative arrangement with reinforced concrete containment structure, Section A-A

- NOTES

- 1 ALL CONCRETE TO DEVELOP 28 DAY COMPRESSIVE STRENGTH MINIMUM OF 3000 PSI
- 2 REINFORCING TO MEET REQUIREMENTS OF A-615 GR 60
- 3 LINER & MATERIAL TO BE A 442
- 4 STRUCTURAL STEEL TO BE A-36

2 REINFORCING TO MEET REQUIREMENTS
OF A-615 GR 60

3 LINER R MATERIAL TO BE A 442

4 STRUCTURAL STEEL TO BE A-36

Fig. 1.40--Reactor building alternative arrangement with reinforced concrete containment structure, details

Fig. 1.41--Model of containment structure

No.	Frequency (cps)	Direction
1	4.39	Horizontal
2	11.43	Horizontal
3	12.78	Vertical
4	21.02	Horizontal
5	23.27	Horizontal
6	30.96	Horizontal
7	34.61	Vertical
8	35.00	Horizontal
9	39.17	Horizontal
10	41.98	Horizontal

Combined modal shear and moment diagrams are shown in Figs. 1.42 and 1.43.

In the internal pressure analysis, the dome and cylinder are treated as thin-walled shell structures that permit a membrane analysis. Since the thickness of the dome and cylinder is small in comparison with the radius of curvature and since there are no discontinuities, such as sharp bends, in the meridional curves, the stresses resulting from pressure are calculated by assuming that they are uniformly distributed across the thickness. The base slab is treated as a flat circular plate supported on a rigid nonyielding foundation.

Discontinuity stresses occur at changes in section or direction of the containment shell. The juncture of the cylinder to the dome is a point of discontinuity, since the cylinder will tend to increase in diameter somewhat differently than the dome under the internal pressure and temperature design conditions. To compute the unrestrained dimensional changes, the dome and cylinder have been considered as steel membranes equivalent to the area of reinforcing steel in the hoop direction. The unrestrained radial deformation of the dome and cylinder are nearly equal; the discontinuity moments and shear are therefore, insignificant, and there is no reinforcing required at the dome to cylinder juncture because of the discontinuity effects.

The juncture of the cylindrical wall and the base mat is also a point of discontinuity. In determining the discontinuity moments and shears, the base mat was considered as offering complete fixity; the only discontinuity, therefore, is that due to the unrestrained radial expansion of the cylinder. As for the dome to cylinder juncture, the unrestrained radial expansion of the cylinder has been computed by considering the cylinder to be a steel membrane equivalent to the area of reinforcing steel in the hoop direction.

For the base, the bending moments, shears, and deflections are found by considering a long cylindrical shell fixed at one end with a uniform internal pressure.

The problem of determining the discontinuity moment and shear at the spring line is similar to that at the base. Discontinuity forces at the dome-cylinder junction are a function of the relative deformation at this point.

Fig. 1.42--Elevation versus shear for reinforced concrete containment structure

Fig. 1.43--Elevation versus movement for reinforced concrete containment structure

In calculating the discontinuity effects, the bending is of a local character so that an approximate solution can be obtained by assuming that the bending is of importance only in the zone of the dome close to the spring line and that this zone can be treated as a portion of a long cylindrical shell.

The temperature effects in the structure are internal in nature: tension on the outside surface and compression on the inside surface of the shell (see Fig. 1.44). The resultant force is zero. Loading combinations (2) and (3) on Fig. 1.44 concurrent with these temperature effects may cause local stresses in the outside horizontal and vertical rows of bars to reach yield (4); however, as local yielding is reached, any further load is transferred to the unyielding elements (5).

At the full-yield condition, the magnitude of the final load resisted across a horizontal and vertical section remains identical to what would be carried if the temperature effects were not considered. Thus, the over-all carrying capacity of the structure and the factor of safety of the structural elements are not affected by the temperature gradient.

Under accident conditions the thermal expansion of the plate, relative to the slower reacting concrete, creates compressive strain in the liner beyond yield. The liner will buckle locally. An angle attachment system is provided to prevent any local buckling from developing into progressive failures of the anchorages and large uncontrolled buckling.

The reinforced concrete containment design is essentially complete. A construction sequence and schedule and a cost analysis are being developed.

Prestressed Concrete Containment. The prestressed concrete containment design is essentially complete. A construction sequence and schedule and a cost analysis are being developed.

Steel Containment with Reinforced Concrete Shield Wall. The steel containment has been designed as a free-standing shell with a composite steel-reinforced concrete base. The cylinder wall is 1-1/2 in. thick and the dome is 3/4 in. thick. The bottom liner is 1/4 in. thick and is anchored to a 7-ft-thick reinforced concrete mat. The reinforced concrete shield wall, which acts as a tornado barrier and a biological and missile shield, is 2 ft thick.

The 1-1/2-in. side wall thickness has been set as the maximum thickness, as thickness exceeding 1-1/2 in. require postweld stress relieving.

Design, fabrication, and erection of the steel containment shell is in accordance with ASME Section III Class B, Nuclear Vessels Code. Material has been selected as A-516-60 to A-300, which meets the requirements of Section III Subsection B.

Figure 1.45 shows the over-all building geometry and size. Still to be resolved is whether the biological shield wall is required for postaccident biological shielding.

A cost analysis is being finalized with the inclusion of the reinforced concrete shield wall, and a construction sequence and schedule are being developed.

Fig. 1.44-Stress redistribution

SECTION A-A

Fig. 1.45--Reactor building alternative arrangement with steel containment structure and reinforced concrete shield wall, Section A-A

SYSTEMS DESIGN

The process flow diagram and system description have been prepared for the reactor plant ventilation system with separate secondary containment and will shortly be issued for review. The design criteria for this case and also for the ventilation system (Base Case) have been completed.

The piping and instrument diagram and system description for the PCRV auxiliary piping system with separate secondary containment have been completed.

Work is under way on the following cases for the secondary coolant system:

1. Steam-to-steam reheat.
2. No reheat.
3. Gas reheat with electric motor circulators.
4. Gas reheat with series circulator turbine after the superheater.
5. Gas reheat with series circulator turbine before the final superheater.
6. Gas reheat with condensing circulator turbines.
7. Gas reheat and separate secondary containment.
8. Gas reheat, reactor-only PCRV.

Process flow and piping and instrumentation diagrams have been prepared for the following circulator auxiliary system alternative cases:

1. Circulator electric motor drive.
2. Steam-to-steam reheat.
3. Split superheater, in-line.
4. Single superheater, in-line.
5. Condensing turbine drive.

Work has been started to prepare a pipe size code. This will be expanded from the existing PSC code to yield pressure drop per unit length of pipe, flow area, metal area, moment of inertia, section modulus, wall thickness, and other useful output.

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Task III
ANALYTICAL METHODS

HEAT-TRANSFER CODES

The names of the TAC and RAT-3D programs were changed to TAC2D and TAC3D, respectively, in order to provide more meaningful names and indicate that the codes are similar. During the quarter many of the TAC program developments and modifications were completed, including:

1. The programming to improve the instability condition that sometimes arises with radiation across internal coolant channels was completed and debugged for both codes.
2. The modifications to correct the inconsistency in evaluating surface temperatures was completed for both TAC2D and TAC3D. The debugging of the code changes for TAC3D remains to be done.
3. Minor changes in both codes were made to improve its running efficiency, reduce its storage requirements, or make its usage easier. These include changes in the FLODAT subroutine and the selection of the time steps to get iteration printouts at specified times.

FORTRAN V, a new computer language that has become available, has programming advantages over FORTRAN IV and is expected to replace the older language throughout the computer field. To prepare for this, both TAC codes were converted to FORTRAN V. In this conversion, changes had to be made in the DATA, MADATA, and FLODATA subroutines with regard to entering thermal properties. The change was beneficial in that it eliminated the LINK subroutine that was written in a MAP language and was compiler dependent. The programs can now be very easily adapted to most computer installations. Test cases prepared for the conversion will serve as a standard set of test cases for future code developments.

CORE THERMAL PERFORMANCE CODE (FLAC)

A comparative study was initiated to systematically evaluate the efficiency of two iteration schemes used to determine flow distribution in the core. The two methods being investigated are the point-relaxation method used in FLAC and the matrix-inversion method used in FLOPSY. Although initial test problems showed the point-relaxation method to be far superior in terms of computer efficiency, some example problems have been encountered in which the point-relaxation method failed to converge, while the matrix inversion method worked very well.

The parameter that is believed to influence the rate of convergence is the relative magnitude of flow resistances in the individual branches comprising the flow network. The relative magnitude of flow resistances is

defined as the ratio of the maximum individual branch resistance to the minimum individual branch resistance comprising the network. Both of the analytical methods being studied are based on linearized equations defining the branch flow,

$$\Delta P = R(W) * W \quad ,$$

where W = branch flow rate,

ΔP = pressure drop in the branch, and

$R(W)$ = flow resistance, which is a function of flow.

Problems have been set up to investigate a range of relative magnitudes of flow resistance from 1 to 10^9 . Preliminary results indicate that the relative resistance indeed does have a profound effect on the convergence time. The matrix-inversion method appears to be superior at low values of relative resistance, and the point-relaxation method appears to be superior at large values of relative resistance. However, an additional factor appears to have some influence on the convergence time. This factor is the relative magnitude of resistance of each flow path between the network flow source and the corresponding flow sink. Problems are being constructed to obtain more data points on the influence of the individual branch resistances as well as on the influence of the over-all resistance of each flow path.

Task IV
FISSION-PRODUCT MECHANISMS

MATHEMATICAL TREATMENT OF FISSION-PRODUCT TRANSPORT

The FIPER-I code for numerical evaluation of the release of radioactive metallic fission products in an HTGR is based on the solution of the one-dimensional diffusion equation. This code has been programmed for use as a production code.

The finite-difference equations leading to the solution of the two-dimensional diffusion equation, by the FIPER-II code, are being programmed. Concurrently, an analytical solution is being found for a simple but important two-dimensional diffusion problem. The problem involves diffusion through a graphite cylinder that contains a small-diameter tube parallel to the axis. By solving the boundary-value problem and calculating concentration profiles and time-dependent release as a function of various parameters (diffusion coefficients, pore sizes, and pressure isotherms), it will be possible to (1) compare analytically calculated releases with numerically calculated releases and (2) evaluate the fast component in a two-channel diffusion model.

VAPORIZATION OF METALLIC FISSION PRODUCTS FROM GRAPHITE

Further vaporization studies were made to improve the heat of vaporization data and to determine the effects of graphite source material on vaporization. The vaporization of strontium from strontium-impregnated graphite having a particle size of 44 to 74 μm was carried out from a molybdenum Knudsen cell in the mass spectrometer. The initial loading of about 5 mg Sr/g C (as the nitrate) was reduced to about 0.02 mg Sr/g C during the course of the experiment. Temperatures of 1000° to 1800°F were involved.

The enthalpies and entropies, derived from the Clausius-Clapeyron equation

$$4.575 \log_{10} P = - \frac{\Delta H}{T} + \Delta S \quad , \quad (1)$$

are linear functions of $\log_{10} C$ (see Figs. 4.1 and 4.2). The least-squares lines are given by

$$\log_{10} C = 2.96 - 0.0477 \Delta H \quad , \quad (2)$$

$$\log_{10} C = 6.27 - 0.400 \Delta S \quad , \quad (3)$$

Fig. 4.1--Dependence of the concentration upon the enthalpy for vaporization of Sr from graphite

Fig. 4.2--Dependence of the concentration upon the entropy for vaporization of Sr from graphite

where C is in mg Sr/g C, ΔH is in Kcal/mole, and ΔS is in e.u. From Eqs. (1), (2), and (3), the Freundlich isotherm is obtained:

$$\log_{10}(P/P_0) = (-0.572 + \frac{4590}{T}) \log_{10}(C/C_0) \quad , \quad (4)$$

with $\log_{10} P_0 = 1.81$ and $\log_{10} C_0 = 2.96$. The pressures are given in atmospheres; the temperatures are in °K. This direct derivation of Freundlich behavior from experimental information is different from the mere assumption of Freundlich behavior made in previous treatments.

Some question has been raised concerning the form of strontium present during the experiment. Experimental evidence indicates that the decomposition of the $\text{Sr}(\text{NO}_3)_2$ to SrO is complete at about 850°K, but the presence of CO throughout a large part of the experiment suggests that at least a part of the strontium present is in the form of the oxide. It is not clear, then, whether a valid description of the data should be given in terms of the reaction

or the reaction

or a combination of both reactions (5) and (6).

Further experimentation to resolve this question may take two directions. First, a gas-fed Knudsen cell may be employed to provide a controlled CO pressure. Second, an attempt may be made to use an oxygen-free Sr-C preparation. It has been shown in a separate experiment that little of the CO observed originates from the graphite.

The dependence of the pressure upon the concentration with the temperature as the parameter is shown in Fig. 4.3. The curves in this figure were generated using Eq. (4) and extrapolated to low concentration assuming no transition to Langmuir behavior.

These data agree, within the uncertainty of the data, with data presented in the previous quarterly report obtained using another type of graphite, indicating no measurable effect of changing the type of graphite.

The heat of vaporization (enthalpy) data, shown in Fig. 4.1 are quite satisfactory. The data are linear on the $\log C$ versus ΔH (Freundlich) plot, in contrast to earlier data (see Fig. 4.2 of earlier quarterly report GA-8530), which showed much more scatter and consequently were much more uncertain. Thus, the recent data tend to satisfy a goal of this task, namely, the reduction of the uncertainty in the heat of vaporization data for strontium sorbed on graphite.

Fig. 4.3--Dependence of the pressure on the concentration for vaporization of Sr from graphite (37- to 34- μm grain)

FISSION-PRODUCT DIFFUSION STUDIES

In studies of the diffusion of metallic fission products in graphite a strontium permeation experiment with graphite at 1000°C has been terminated after 2050 hr of heating, and the data are being analyzed. A description of the apparatus was presented in the previous quarterly report (GA-9227).

Task V
RECYCLE FUEL STUDIES

INTERIM STORAGE OF SPENT HTGR FUEL ELEMENTS

A topical report on the interim storage of HTGR fuels discharged from reactors prior to the availability of HTGR fuel reprocessing is being prepared for publication (Ref. 1). The previous quarterly report (GA-9227) contains a detailed discussion of the study results.

HTGR FUEL REFABRICATION STUDIES

The topical report on a central refabrication plant for HTGR fuels was published (Ref. 2). The results of the study are contained in an earlier quarterly report, GA-9090. The study on which the report is based is being expanded to consider the effect of blending thorium with the U^{233} . A straight U^{233} fuel particle was employed in the basic study.

A study has been initiated to estimate a breakdown of costs associated with various steps in HTGR fuel refabrication; these steps are U^{233} particle fabrication, U^{233} fuel element assembly and storage, and U^{235} fuel element assembly and storage.

NATIONAL HTGR RECYCLE DEVELOPMENT PROGRAM PLAN

The National HTGR Recycle Development Plan was revised and rewritten and a fourth draft has been prepared. The parts of the plan developed by Oak Ridge National Laboratory, PERT charts, progress milestones, and revised budgets were incorporated into the plan. The informal comments of the AEC Division of Reactor Development and Technology have been reviewed and appropriate changes made in the fourth draft of the plan. The fourth draft will be issued upon receipt from ORNL of recent changes in program and budget for Section 5300, Materials Irradiation.

LIAISON WITH ORNL

Fuel element design revisions were given ORNL for incorporation into their HTGR Base Program and Thorium Utilization Program. Gulf General Atomic has been asked by ORNL to prepare fuel element samples for their reprocessing studies. Specifications for this special fuel material have been reviewed.

SPENT FUEL SHIPPING STUDIES

Studies on land and ocean shipping of HTGR spent fuel continued. Shipping cask design and operating data were developed and given to the Gulf Oil

Corporation heavy equipment transportation division. Costs for capital equipment and operating expenses for a truck transportation system will be obtained for further study of the spent HTGR fuel shipping part of the fuel recycle.

RECYCLE FUEL ELEMENTS FOR THE PEACH BOTTOM REACTOR

Work has been initiated on the design of eight recycle elements for insertion in the Peach Bottom reactor second core. These elements will resemble recycle fuel as much as possible and will be irradiated to supply samples for the ORNL HTGR fuel reprocessing head-end pilot plant hot operation.

HEAD-END REPROCESSING

Block Crushing

Further experiments have been performed to confirm the initial promising results that crushing and sieving can be used to largely separate graphite from fuel particles. A fuel rod weighing 73.8 g and containing 5.19 g of uranium and 21.94 g of thorium, TRISO fissile/TRISO fertile, carbide cores, encased in a graphite tube weighing 172.6 g, was crushed (1/2-in. jaw gap) and the product sieved. The results are shown in Table 5.1. These results show that a large fraction of graphite can be separated from particles and that the weight of product less than 1/8-in. mesh is small. Because of this, the next rod, encased in a graphite tube, and identical to that in run No. 2, was similarly crushed but only three sieves were used for the screening step. The results, shown in Table 5.2, confirm the findings of run No. 2.

Table 5.1
SIEVE FRACTIONS OF CRUSHED PRODUCT FROM RUN NO. 2
(Jaw Gap = 1/2 in.)

Sieve Size	Graphite (g)	Particles (g)
+1/2 in.	115.03	0
+5/16 in.	42.57	11.93
+1/8 in.	10.55	19.93
Combined		
+835 μm	42.98	
+500 μm	1.20	
+420 μm	0.77	
+300 μm	0.18	
-300 μm	0.82	
Loss	0.44	

Table 5.2

SIEVE FRACTIONS OF CRUSHED PRODUCT FROM RUN NO. 3
(Jaw Gap = 1/2 in.)

Sieve Size	Graphite (g)	Particles (g)
+1/2 in.	106.72	0
+1/8 in.	57.57	35.52
-1/8 in.	4.40	38.28

In a systematic survey of the effect of jaw gap and fines production, four 1-rod fuel elements (i.e., a graphite tube containing one fuel rod) were crushed at ORNL in a 3-in. jaw crusher at four different jaw gaps. The results, given in Table 5.3, show that as the jaw gap is narrowed from 1/2 in., the proportion of graphite that is free of particles decreases and the proportion of -300 μ m fines increases.

Table 5.3

WEIGHT DISTRIBUTION IN PRODUCT AFTER CRUSHING IN A
3-IN. JAW CRUSHER AT THE STATED JAW GAP

Run No.	Jaw Gap (in.)	Product (g)			
		+1/2 in. ^a	+1/8 in.	+300 μ m	-300 μ m
12	1/2	158.74	48.96	33.35	1.03
13	3/8	121.81	69.29	50.42	2.08
14	1/4	53.33	111.04	70.80	5.92
15	1/8	0	130.57	91.94	18.79

^aThe 1/2-in. fraction contained graphite only, no particles.

It is concluded that a 1/2-in. jaw gap is close to the minimum gap desirable to recover a large fraction of particle-free graphite and a low fraction of broken particles (i.e., -300 μ m).

Tumbling. In the conceptual flow-sheet, particles and graphite of a size less than 1/8-in. mesh may be fed to a fluid-bed burner in which lumps of greater than 1/8-in. mesh may present a problem. During the quarter, two 24-in. tumblers (see Fig. 5.1) were installed for breaking down +1/8-in. lumps.

Using the tumblers, the +5/16 and +1/8 mesh product (Table 5.1) was tumbled and sieved repeatedly; the results are shown in Table 5.4. The material remaining in the tumbler at the end of the run was graphite only.

Fig. 5.1--24-in. tumbler

Table 5.4

TUMBLING CRUSHER PRODUCT FROM RUN NO. 2

Time (min)	-1/8-in. mesh		Total Cumulative Weight (g)
	+300 μ m Material (g)	-300 μ m Material (g)	
10	24.17	1.02	25.19
25	5.28	0.76	31.23
45	2.00	1.20	34.43
75	0.18	0.45	34.94
105	0.27	1.05	36.26
135	0.22	0.89	37.28
165	0.07	0.28	37.63
195	0.15	0.32	38.10 ^a
Total	32.34	5.97	---

NOTE: For initial charge 53.2 g of graphite plus 31.8 g of particles gave a total of 84.98 g (+5/16 and +1/8-in. mesh).

^aRemaining material, $84.98 - 38.10 = 46.88$ g, was graphite only.

This experiment shows that a further separation of graphite and particles can be obtained after crushing. The combination of the various mesh size materials leads to the results given in Table 5.5. The initial tube, matrix, and outer PyC (i.e., the burnable carbon) weighed 188.2 g; calculation shows that 86% of run No. 2 was recovered free of particles. The remainder of the fuel element was -1/8-in. mesh, contained all the particles, and could be fed to a fluid bed for burning.

In run No. 3 the tumbler was used in a slightly different manner, in that the particle-free graphite was tumbled in addition to +1/8-in.-mesh material. This variation, if successful, would mean that only one 1/8-in. screen would be required after crushing. The results of the tumbling (see Table 5.6) show that the final weight of particle-free graphite is much less than in run No. 2, because the graphite was also attrited in the tumbler and joined the -1/8-in. mesh material. It was concluded that this variation was unsuccessful and would be considered no further.

The +1/8-in.-mesh mixtures of bonded particles and graphite in run Nos. 12, 13, 14, and 15 (Table 5.3) were separately tumbled for 1 hr and sieved through a 1/8-in. screen. The +1/8-in.-mesh material, which consisted entirely of graphite in each case, was added to the +1/2-in. fraction shown

Table 5.5
COMBINATION OF MATERIAL FROM CRUSHING AND SIEVING
FROM RUN NO. 2

Mesh	Graphite (g)	Particles
+1/8-in.	161.91	0
	Combined	
+300 μ m	77.47	
-300 μ m	6.79	

Table 5.6
TUMBLING CRUSHER PRODUCT

Time (min)	Weight -1/8 in. (g)	Cumulative Weight (g)
10	36.76	36.76
25	5.49	42.25
55	5.02	47.27
85	3.58	50.85
115	3.00	53.85
145	2.78	56.63
175	2.57	59.20
205	2.20	61.40
235	2.87	64.27
265	2.39	66.66 ^a

NOTE: For initial charge 106.7 g of +1/2-in. graphite plus 93.09 g of graphite-particles gave a total weight of 199.81 g.

^aRemaining material, 199.81 - 66.66 = 133.15 g, was graphite only.

in Table 5.3. The results, given in Table 5.7, confirm the findings from run No. 2, that is, a large fraction of the burnable carbon can be recovered free of particles.

Table 5.7
WEIGHT DISTRIBUTION OF PRODUCT AFTER CRUSHING,
TUMBLING, AND SIEVING

Run No.	Jaw Gap (in.)	Product (g)		+1/8 in. Mesh Material ^b
		+1/8 in. ^a	-1/8 in.	
12	1/2	170.12	37.18	83.9
13	3/8	166.01	25.29	81.3
14	1/4	150.38	13.35	75.9
15	1/8	123.11	7.30	66.1

^aThe +1/8-in. fraction contained only graphite, no particles.

^bAs a fraction of total burnable carbon.

Burning. Burning performs two tasks: it removed the outer PyC from the particles to expose the SiC coat so that separation of the fissile and fertile particles can be readily accomplished by screening, and it converts broken particles to a fine ash that can be separated from the whole particles by screening. In order to determine the fraction of particles broken by burning only, some loose particles were burned in an Inconel tray, examined microscopically to count the broken particles, and sieved on a 246- μ m screen; the ash was then collected and weighed. In order to determine the fraction of particles broken by tumbling only, some loose particles were tumbled for 2 and 15 hr, burned, examined microscopically, and screened, and the ash was weighed. The results (see Table 5.8) show that burning possibly causes a small amount of damage to loose particles but tumbling causes none.

The fraction of particles broken during crushing was determined by burning the crusher fines, sieving, and weighing the ash produced. The results are shown in Table 5.9. It can be seen that particle damage is dependent upon jaw gap. The optimum jaw gap under the conditions of the experiments is 1/2 in.

The +1/8-in. and +5/16-in. material (see Table 5.1) was tumbled and sieved through a 1/8-in. and a 300- μ m screen. The results (given in Table 5.10) show that although 99% of the residual weight was recovered after tumbling for 45 min, particles were still being recovered after 195 min.

The separation of particles is achieved by sieving the SiC-coated particles through a 420- μ m screen that allows the fissile particles to fall through but retains the fertile particles. This operation has been carried

Table 5.8

PARTICLE BREAKAGE DURING BURNING AND DURING TUMBLING AND BURNING

Run No.	Particle Type	-246 μ m Material (%)		Weight Loss (%)	
		After Burning	After Tumbling and Burning	After Burning	After Tumbling and Burning
16	Fissile	0	0 ^a	25.0	25.3
	Fertile	0	0 ^a	18.2	18.2
17	Fissile	0.22	0.29 ^a	27.4	27.5
	Fertile	0.24	0.23 ^a	17.0	17.0
18	Fissile	0	0 ^b	25.3	25.2
	Fertile	0.12	0.04 ^b	18.0	19.0

NOTE: 24-in., 24-rpm tumbler; 750°C burning temperature; initial sample contained ~10 g of loose particles.

^aTumbled for 2 hr.

^bTumbled for 15 hr.

Table 5.9

URANIUM AND THORIUM IN ASH LEFT AFTER BURNING CRUSHER FINES

Run No.	Jaw Gap (in.)	Weight of -300 μ m After Crushing and Burning (g)	Weight of Uranium (g)	Weight of Thorium (g)	Total U in Ash (wt-%)	Total Th in Ash (wt-%)
2 ^a	1/2	0.040	0.0127	0.0003	0.25	0.003
12 ^b	1/2	0.0393	0.0034	0.0245	0.15	0.10
13 ^b	3/8	0.0716	0.0052	0.043	0.23	0.18
14 ^b	1/4	0.3155	0.0138	0.174	0.62	0.71
15 ^b	1/8	0.93	0.086	0.530	3.85	2.16

^aWeight of U in initial fuel rod = 5.19 g; weight of Th in the initial fuel rod = 24.96 g.

^bWeight of U in initial fuel rod = 2.226 g; weight of Th in initial fuel rod = 24.50 g.

Table 5.10

WEIGHT CHANGE OF BURNING TUMBLED MATERIAL FROM RUN NO. 2
(Initial Charge: +1/8 and +5/16 in., Table 5.1)

Weight Before Burning (g)	Weight After Burning ^a (g)	Microscopic Examination
+300 mesh:		
24.17	}	Approx. 50 yellow particles; some white spheres (broken particles)
5.28		
2.00		
0.18	0.050	1 white particle in transparent SiC shell
0.27	0.120	White ash; many whole particles
0.22	0.012	White ash, 38 fertile, 117 fissile particles
0.07	0.003	White ash, 3 fertile, 9 fissile particles
0.15	0.009	White ash, 19 fertile, 29 fissile particles
-300 mesh:		
1.02	0.010	White ash, 4 fissile particles
0.76	0.004	Brown ash, 2 particles, 5 SiC shells
1.20	0.004	Brown ash, 1 particle
0.45	ND ^b	White ash
1.05	0.004	White ash
0.89	ND	White ash
0.28	ND	White ash
0.32	ND	White ash

^aTotal weight = 21.756.

^bND = not determined.

out a number of times; the results are given in Table 5.11. Analysis of both the fertile and fissile particles for uranium and thorium enables the crossover of each into the other to be calculated. These results are also given in Table 5.11. It can be seen that the observed recovery weights are close to those calculated. The crossover is fairly small at less than 4%, but the values will depend on the largest fissile and smallest fertile particle sizes.

Table 5.11

RECOVERY OF FISSILE AND FERTILE PARTICLES BY SCREENING AND CROSSOVER OF FISSILE TO FERTILE AND FERTILE TO FISSILE^a

Run No.	Weight Recovered (g)		Crossover (%)	
	Fissile Material	Fertile Material	Fissile to Fertile	Fertile to Fissile
2	16.00	34.78	3.0	----
3	17.34	34.31	2.0	----
4	17.52	32.81	1.7	0.35
6	17.64	34.90	4.1	0
Calculated Recovery, g	17.03	35.26	----	----
12	23.43	18.00	<0.8 ^b	(c)
13	23.36	18.21	<0.8 ^b	(c)
14	23.46	17.75	<0.8 ^b	(c)
15	21.95	17.80	~0.8	(c)
Calculated Recovery, g	21.78	17.82	----	----

^aScreen size was 420 μm , which may not be optimum for these particles.

^bLimit of detection for U in Th (in the particular method of analysis used).

^cFertile particles were BISO; hence, all were burned to powder. The crossover of fertile to fissile was therefore not measured.

Grinding and Leaching. A double-roll grinder and a one-liter continuous leacher have been fabricated for testing.

Discussion. The research and development is continuing in the direction of the conceptual flowsheet shown on Fig. 5.2, in which the values are based on the results from run No. 12.

Fig. 5.2--Simplified flowsheet of the crushing and tumbling concept based on the results of run No. 12

The 1/2-in. jaw gap gives low particle breakage because the fuel rods are 1/2 in. in diameter. When the graphite tube is broken between the jaws, the rod inside is crushed and has a good chance of dropping right through the jaw gap without being crushed a second time. The first crushing action tends to shatter it so that none remains on the 1/2-in. screen.

The effect of jaw gap on particle breakage is shown on Fig. 5.3, and the effect on the separation of burnable carbon (graphite tube, matrix, and outer PyC) after crushing and tumbling is shown on Fig. 5.4. It is almost certain that the fraction of fertile particles broken in any given operation is not the same as that for fissile particles; as firm evidence is obtained, the results will be reported. Figure 5.4 shows that despite the fact that the burnable carbon increases after tumbling, the sum of the crushed and tumbled fractions is decreasing. Figure 5.5 shows the increase in the fraction of carbon fines retained with the particles, and Fig. 5.6 shows the carbon accompanying the broken particles.

It was considered that the attrition rate in the tumbler may be first order, so the results of run Nos. 2, 3, and 6 were plotted and are shown in Fig. 5.7, where f is the fraction percent of -1/8-in. material. The curves show that the particle agglomerates are attrited very much more rapidly than the graphite, whose rate is the straight line remaining after the particle agglomerates have been removed in the product. Although the attrition rate of the graphite is first order in the time span of the tumbling experiment, the three straight lines do not agree even though the same type of graphite was used in all cases. It is considered that this is due to the fact that the lumps of graphite are not spherical. However, further tumbling experiments will be done to confirm this.

Microscopic examination after burning revealed two faulty types of particle that appeared to be yellow instead of black. Radiography showed that a cracked SiC coat allowed air into the interior, which was oxidized to white ThO_2 , and through the slightly yellow SiC coat, the particle appeared to be yellow. The other type was a double-SiC-coated particle with the outer SiC cracked.

All results indicate that the route depicted on Fig. 5.2 is possible. Crushing with a jaw crusher and tumbling, although slightly damaging to particles, can be considered candidates for head-end treatment. Burning statically does virtually no damage to the particles, and separation by screening appears to perform a fairly clean job, allowing less than 4% overlap.

Broaching and Splitting

The objective of broaching and splitting is to separate the fuel rods from the block. It is an alternative to crushing the fuel element. A tubular broach, 5/8 in. in outside diameter (upper photo in Fig. 5.8), is pushed into a coolant hole, and the cutters groove the graphite toward the fuel holes (Fig. 5.9). After the broach is removed, the splitter (lower photo in Fig. 5.8) is pushed down the coolant hole while the fuel element is restrained to prevent it from falling apart, and the thin graphite web between the bottom of each groove and fuel hole is broken. When the splitter is removed, the fuel element is allowed to fall apart. The graphite is in the form of sticks, and the fuel rods are recovered as short lengths of rod.

Fig. 5.3--Particle breakage during jaw crushing (run Nos. 2, 12, 13, 14, and 15)

Fig. 5.4--Separation of carbon after crushing and tumbling (run Nos. 12, 13, 14, and 15)

Fig. 5.5--Retention of carbon with particles after crushing and tumbling (run Nos. 12, 13, 14, and 15)

Fig. 5.6--Carbon in the -300- μ m sieve fraction after crushing (run Nos. 12, 13, 14, and 15)

Fig. 5.7--Attrition by tumbling

Fig. 5.8--Components of broaching and splitting system: (above) braided broach showing one set of three pairs of cutters; (below) splitter

Fig. 5.9--End view of broached test piece; brazed broach was used, and wooden dowels were used in the fuel holes; grooves on hexagon corners cut with band saw

It is envisioned that numerous broaches would be used in a full-scale facility because there are 108 coolant holes to be broached in 1/2 hr.

Design Requirements. The more important initial design factors to be determined are:

1. The pressure needed to force the broach through the coolant hole.
2. The depth and width of the groove and the shape of the groove cross section.
3. Whether the broach should be pushed or pulled.
4. The web thickness, considering that the coolant and fuel holes can drift ± 0.030 in. along the 30-in. length of the block and still be within specification.
5. Whether to use interchangeable or brazed cutters on the broach.
6. The life of the cutter before the force required to push (or pull) the broach becomes excessive.
7. Whether to use tube or rod broaches.
8. The type of splitting tool to be used.
9. The type of restraint needed during the splitting to prevent the fuel element from falling apart.

Broaches. Three broaches have been fabricated and tested. The first was a stainless steel tube 5/8 in. in diameter with 24 cutters brazed to the outer circumference (see Fig. 5.8). The cutters were 0.162 in. wide and were arranged in four sets of three pairs. The first set to enter the coolant hole measured about 0.71 in. across the cutting edges; the second 0.79 in., the third 0.90 in., and the fourth 0.95 in.

The broach, when pushed through a small fuel block, cut grooves as shown on Fig. 5.9. The grooves were found to be too deep and the graphite web was breaking away, so the fourth set of cutters was ground down to 0.93 in. across cutting edges.

The second type of broach was a stainless steel tube in which 12 square-slotted tool holders had been brazed across the diameter, so that a cutter could be fabricated from a 3/16-in. square bar and fitted into the slotted tool holder. Each cutter was held in position with an Allen set screw. The objective was to test the feasibility of using a broach that had interchangeable cutters, so that when the cutting edge became worn, the cutters could be replaced instead of replacing the whole broach.

The third type of broach was similar to the second in that interchangeable cutters were used, but the broach was fabricated from an hexagonal bar of mild steel. The objective was to provide a broach with greater rigidity and strength than that provided by a tube.

Broaching Trials. The brazed broach was tested, and the force required to push it through a coolant hole was measured (see Table 5.12). The results show that the force required to push four sets of three pairs of cutters through a coolant hole lies between 1400 and 1940 lb. A change of cutting speed from 2.8 to 5.6 cm/sec had no effect on the force.

Table 5.12
FORCE REQUIRED TO PUSH ONE SET OF SIX CUTTERS
ON BRAZED BROACH THROUGH A COOLANT HOLE

No. of Coolant Hole	Force Required to Push One Set of Cutters (lb)	
	Minimum	Maximum
1	350	400
2	375	450
3	350	525
4	300	525
5	325	475
6	350	575
7	400	450
Mean	350	485

The second broach (tube with interchangeable cutters) was tested and found to have the following disadvantages:

1. The Allen screw hexagons filled with graphite powder and the key could not be inserted until the graphite was cleared by using a pin.
2. The cutters had to be hammered out of the tool holders because they had become jammed in with graphite powder.

Another disadvantage foreseen but not experienced is that the tool holders fill in a large fraction of the cross section of the tube, which could lead to graphite chips piling up instead of dropping clear.

The third broach (hexagonal bar with interchangeable cutters) was tested, but the gap between the hexagonal bar flats and the coolant channel wall was not sufficient to allow the graphite chips to fall clear; the broach jammed in the channel and had to be removed by reversing its motion.

Splitting. Trials with splitters have shown that the operation is simple provided the fuel element is not allowed to fall apart as splitting proceeds. If the element is allowed to fall apart, the splitter is displaced from the center of the coolant hole and from then on, only two opposite webs are split instead of six. The restraint in the trials to date has been applied by three rubber bands around the top, center, and bottom of the 6-in.-long test piece.

The restraint on the test piece has to be removed before the splitter is withdrawn; otherwise, the splitter catches on the split pieces of graphite and tends to disrupt the whole test piece.

STABILITY OF TRISO COATED PARTICLES DURING FLUID-BED OXIDATION

Studies of the stability of unirradiated and irradiated TRISO coated fuel particles during fluid-bed oxidation have continued. The current effort is directed toward determining the failure fractions of particle lots irradiated in the P13F, P13H, and P13J capsules during fluid-bed oxidation, and comparing these results with those of similar tests on unirradiated samples of the same particle lots. Because it was not possible to use the samples closest to the current reference design particles for these studies, it has been necessary to use samples with thinner-than-reference SiC layers. It is likely, therefore, that the results of the current studies will show higher failure rates than those eventually anticipated.

Initially, two samples were selected from capsule P13H: one a TRISO-I particle (3325-113E) and the other a TRISO-II design (3325-125E). Preliminary experiments using a sample of unirradiated material of the TRISO-I design showed highly abnormal behavior on static bed oxidation, with a high fraction of failed particles. Consequently, the as-received (unirradiated) material was examined more carefully and found to contain significant fractions of defective particles, coating shells, graphitic debris, and tramp material. The nonspherical material was separated from the fuel particles by tabling and found to constitute 1.5 wt-% of the sample. The fractions were separately subjected to static bed oxidation, and the "particle sample" (as distinct from the debris) was tabled again to remove failed particles. The failed particles, the remaining intact sample, and the preoxidation debris were leached separately to determine the amount of leachable uranium in each fraction. These data are given in Table 5.13.

Table 5.13

LEACHABLE URANIUM IN SAMPLE FRACTIONS OF
UNIRRADIATED PARTICLE LOT 3325-113E

Sample	Fraction of Total Sample (wt-%)	Leachable Uranium	
		Percent of Fraction	Percent of Total U in Sample
Debris in as-received material before oxidation	1.5 ^a	0.038	0.000570
Debris tabled from particle sample after oxidation	10.5 ^b	0.040	0.0034
Particle sample (all debris removed)	89.5 ^b	---	0.047

^a-Before oxidation.

^b-After oxidation.

Investigation of the TRISO-I lot of fuel particles (3325-113E) by metallographic examination of the SiC layer showed an eroded appearance. It was concluded that this lot of particles was defective before irradiation and that studies of the irradiated material would tell nothing about the effects of irradiation on it. It should be noted that this sample showed a postactivation release of Sr⁹¹ of 1×10^{-3} before irradiation, while the in-pile release of Sr⁸⁹ was 3.5×10^{-2} . Further work with this material seems fruitless and has been discontinued.

Because of the unusual behavior of the TRISO-I material discussed above, a similar series of experiments was carried out on the TRISO-II sample (3325-125E). Approximately 1% debris, separated from the unirradiated sample by tabling, was oxidized and found to contain 0.001% of the total uranium. The intact particles were also oxidized statically, and subsequent microscopic examination showed no failed particles. The sample was then leached to determine the amount of unprotected uranium, and 0.0001% was found in the leach solution.

In addition to the static bed studies, four samples from lot No. 3325-125E have been oxidized in a 1-in.-diameter fluidized bed for periods up to 48 hr at 850°C. Before oxidation, the particles were separated from the debris by tabling. The oxidized particles, debris, and bed were then leached to determine the fraction of exposed uranium. These data are given in Table 5.14. Experiments with irradiated samples are currently being performed.

Table 5.14

LEACHABLE URANIUM IN SAMPLE FRACTIONS OF
UNIRRADIATED PARTICLE LOT 3325-125E

Sample	Leachable Uranium	
	Percent of U in Tabled Debris	Percent of Total U in Sample
Debris tabled from following samples	0.011 ^a	0.00014
Tabled particles	---	0.00017
Tabled particles	---	0.00006
Tabled particles	---	0.00011
Tabled particles	---	0.00013

^a-Fraction represents 1.3% of combined samples.

References

1. Shefcik, J. J., Interim Storage of Spent HTGR Fuel, USAEC report GAMD-9272, Gulf General Atomic Incorporated, to be published.
2. Stelling, H. E., Central Refabrication Plant for HTGR Fuels, USAEC report GAMD-9145, Gulf General Atomic Incorporated, March 15, 1969.

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Task VII
EXPERIMENTAL PHYSICS

LARGE CONTROL ROD EXPERIMENTS IN LATTICE ASSEMBLY NO. 2

Measurements and analyses were completed to determine the worth of hafnium carbide relative to that of boron carbide as an HTGR control material. The measurements were made with hafnium carbide loaded in a 3.5-in.-diameter cylindrical tube along the axial centerline of lattice assembly No. 2, which has been described in earlier quarterly reports. Three hafnium carbide loadings were used. For each hafnium carbide loading, the boron carbide loading in the same size tube that would give the same reactivity worth was determined. Graphite in the form of coke was used to change the concentration of hafnium or boron in the rod. The loadings are given in Table 7.1.

For these large rod measurements, the rod was inserted into a critical core loading (base case) and then the resulting core loading changed by increasing the core radius and the outer reflector radius. For each of these hafnium and boron measurements (six in all) the core radius was increased by the addition of 152 fuel elements and core criticality was then adjusted by shimming the reflector thickness. Results of these measurements are shown in Table 7.2. The reactivity difference between the cases was obtained experimentally.

For these measurements, the absolute worths of the hafnium rods could not be measured directly but were measured relative to comparable boron loadings in identical cylindrical containers. In earlier measurements for similar boron rods, it was shown that the calculated worth for a single boron rod was within 4% of the measured worth. In those measurements, the boron density in the rod was about 1.0 g/cm^3 , whereas the equivalent boron loading for comparison with the hafnium-loaded cylinders was between 0.04 and 0.2 g/cm^3 . This reduced boron density should have little effect on the calculational agreement. Therefore, the absolute worth of the hafnium was obtained from the calculated worth of the measured equivalent boron to within 5%. Some additional measurements were made to substantiate the reactivity difference between cylinders with different hafnium loadings.

The calculations for the reactivity worth were made exclusively with transport theory. Two nuclear models were employed in these calculations:

1. Full-core radial calculations in which the large hafnium rod surrounded by a graphite sleeve was located at the center of the lattice. Natural hafnium cross sections averaged over the lattice spectrum were employed. This does not account for self-shielding in the energy domain of the hafnium resonances, and consequently broad-group cross sections that are too high are used for the hafnium.

Table 7.1

LOADING OF HfC AND B₄C IN 3.50-IN. TUBE

Loading	Hafnium Carbide Rods			Boron Carbide Rods		
	Hf-1	Hf-2	Hf-3	B-1	B-2	B-3
HfC or B ₄ C mass (kg)	46.00	16.794	8.396	1.914	0.638	0.542
Volume (liters)	10.18	10.31	10.31	10.35	10.32	10.32
ρ_{HfC} or $\rho_{\text{B}_4\text{C}}$ (g/cm ³)	4.52	1.62	0.815	0.185	0.0618	0.0525
ρ_{Hf} or ρ_{B} (g/cm ³)	4.14	1.49	0.745	0.145	0.0484	0.0411
Coke mass (kg)	5.00	10.89	11.84	10.05	11.31	11.41
ρ_{coke} (g/cm ³)	0.49	1.057	1.149	0.974	1.096	1.106

Table 7.2

RESULTS OF CRITICAL MEASUREMENTS ON LARGE RODS

Loading	Hf or B Density (g/cm ³)	Reflector Elements Added	Measured Core Reactivity (\$)	Core React. with 368 Reflector Elements (\$)	$\Delta\rho$ (\$)
Hf-1	4.14	368	0.054	0.054	0.000
Hf-2	1.490	312	0.007	1.099	1.045
Hf-3	0.745	284	0.344	2.052	1.998
B-1	0.145	368	0.224	0.224	0.000
B-2	0.0484	312	0.496	1.588	1.364
B-3	0.0411	284	0.182	1.890	1.666

2. Full-core radial calculations in which the hafnium is homogenized into the lattice region. For these calculations effective broad-group cross sections as obtained from a resonance calculation were used in the resonance region and homogenization (g) factors were used in the thermal range.

The broad-group cross sections used in the analysis with both models are shown in Table 7.3. In addition, the effective broad-group cross sections for various loadings of hafnium obtained using the self-shielding factors from the explicit calculation (Model 1) are shown. Note that the effective absorption cross section is nearly independent of loading for the thermal and epithermal groups.

The results of the experimental and analytical studies for these large rods of hafnium are summarized in Figs. 7.1 and 7.2. In Fig. 7.1, the calculated reactivity worth for cylinders loaded with different concentrations of boron is shown. The other curve is the reactivity worth for the hafnium-loaded cylinders based on the equivalent measured boron loading. The measured reactivity difference between the different hafnium loadings is also shown, normalized to the loading with 1.5 g/cm³. Figure 7.2 shows the same measured hafnium worth curve as compared to the calculated worth curves using different analytical models.

From the results shown in these figures, the following conclusions can be made with regard to the reactivity worth for large cylinders of hafnium or hafnium carbide:

1. The absolute values for reactivity worth of the hafnium loaded rods as obtained from the calculated worth of equivalent boron rods are accurate to about 5%. This is based on the previous analysis for lumped boron worth and the comparison with the direct measurements for $\Delta\rho$ between the different hafnium loadings.
2. The best agreement between the calculated and measured reactivity worth for hafnium was obtained using the model with the infinitely dilute hafnium cross sections in the explicit transport theory analysis. This is shown in Fig. 7.2, where the calculated worth is too high by only about 5%.
3. The agreement between measured and calculated worths using this model is similar for all hafnium densities of interest, between 1.0 and 6.0 g/cm³.
4. Resonance calculations for obtaining effective broad-group hafnium cross sections in the resonance region are inadequate for this large cylinder. This inadequacy can be seen in Fig. 7.2, where the calculated worth, using this nuclear model, deviates significantly from the measured worth.
5. For cylindrical rods of this size, approximately 20 times (by weight) as much hafnium is required to give the same reactivity worth as a given amount of boron.

Table 7.3
HAFNIUM CROSS SECTIONS FOR LARGE ROD

Broad Group Cutoff Upper E (ev)	Model 1 Natural Hf abs. Cross Section (Inf. Dilute) (barns)	Effective Cross Section, Σ_a (cm ⁻¹)			
		$\Sigma_a = Ng\sigma_a$			Model 2 Cross Sections from Res. Calc., $\rho_{Hf}=4.15$
		$\rho_{Hf}=0.75$	$\rho_{Hf}=1.50$	$\rho_{Hf}=4.15$	
14.9x10 ⁶	0.019	0	0	0	0
1.35x10 ⁶	0.075	0	0	0	0
1.83x10 ⁵	1.209	0.0001	0.0001	0.0003	0.0004
961.1	7.714	0.0004	0.0007	0.0011	0.0008
214.4	32.239	0.0010	0.0014	0.0015	0.0025
47.85	284.810	0.0016	0.0015	0.0013	0.0053
2.38	526.620	0.0014	0.0014	0.0013	0.0013
0.414	49.062	0.0008	0.0010	0.0012	0.0012
0.10	70.546	0.0010	0.0011	0.0012	0.0012
0.04	117.420	0.0012	0.0013	0.0012	0.0012

Fig. 7.1--Reactivity worth for 3.5-in.-OD cylinder versus poison density in rod

Fig. 7.2--Calculated reactivity worth for hafnium in large rod versus hafnium density

6. The increased worth for the boron-loaded rods is also evident from the data shown in Table 7.4. As for the case with hafnium, the increased absorptions occur at the higher energies, with the major increase coming from the group between 961 ev and 183 kev. Note the data for the boron and hafnium loading of equal worth (approximately \$8.30) and that for the increased boron loading giving the higher reactivity worth, \$9.10.

PARTICLE SIZE EFFECT ON THORIUM DOPPLER COEFFICIENT

Measurements are currently under way to determine the thorium Doppler coefficient for three different thorium carbide particle sizes, 125, 450, and 600 μm . The C/Th ratio for all three cases is 300. The thermal base effect, in which the thorium is replaced by an amount of boron equivalent to the $1/v$ absorptions in the thorium, is also being measured.

BURNABLE POISON PIN INTERACTION

A series of measurements has been performed to obtain data on the interaction of burnable poison pins in the central lattice fuel element. The poison pins were Pyrex glass rods that have a boron content of 1.26 g per 72 in. core length. Each rod is 0.35 in. in diameter.

Reactivity worth measurements were made with a single pin at the center of the lattice and at two radii to determine the flatness of the flux through the central element. Reactivity worth measurements were then made with two and with three pin clusters using various separations. Figures 7.3 and 7.4 present the results of these measurements.

For the calculated values, an eigenvalue was determined for the Base Case, which had no poison, using 10-group diffusion theory. A one-dimensional transport-theory calculation was used to define the single poison pin at the center of the lattice, and a two-dimensional transport-theory calculation was used to define the three poison pins at the various spacings. Because of the limited number of mesh points available, the direct results from this calculation were unsatisfactory. The final analysis was done by reducing the model to include a central cell region surrounding the poison pins. Self-shielding factors were then calculated for the poison pins, and these factors were used to modify the boron cross sections that were used in the Base Case calculations. The boron was then smeared into the central zone of the radial base calculation using the modified boron cross sections for the final results. The calculated results are given in Fig. 7.3 and are presented graphically in Fig. 7.4. Agreement with experiment is excellent.

Table 7.4

BROAD-GROUP ABSORPTIONS FOR LARGE RODS WITH Hf AND B
(Explicit Transport Theory Model)

Broad Group Upper E Cutoff (ev)	Large Hafnium Rod		Large Boron Rod		
	$\rho_{\text{Hf}}=0.75$	$\rho_{\text{Hf}}=4.15$	$\rho_{\text{B}}=0.05$	$\rho_{\text{B}}=0.257$	$\rho_{\text{B}}=0.515$
14.9×10^6	0	0	0	0	0.0001
1.35×10^6	0	0.0001	0.0001	0.0002	0.0004
1.83×10^5	0.0009	0.0042	0.0012	0.0048	0.0078
961.1	0.0013	0.0039	0.0011	0.0033	0.0043
214.4	0.0034	0.0047	0.0019	0.0040	0.0042
47.85	0.0083	0.0068	0.0060	0.0071	0.0068
2.38	0.0043	0.0039	0.0045	0.0040	0.0038
0.414	0.0031	0.0041	0.0050	0.0045	0.0043
0.10	0.0026	0.0028	0.0031	0.0029	0.0028
0.04	0.0013	0.0012	0.0013	0.0012	0.0012
Calc. $\Delta\rho$ (\$)	6.60	8.26	6.52	8.30	9.10

Poison Pin in Location	Total Worth (\$)	Δp (\$)	Increase (%)	Calc. (\$)	Radius (in.)	Spacing (in.)
1	0.7861	0.0000	- - -	0.810 ^a	0.0000	- - -
13	0.7867	0.0006	0.07	- - -	1.300	- - -
26	0.7915	0.0054	0.69	- - -	2.250	- - -
1, 9	1.4267	0.0000	- - -	- - -	- - -	1.300
13, 9	1.4745	0.0478	3.35	- - -	- - -	2.250
26, 20	1.5169	0.0902	6.32	- - -	- - -	3.900
1, 9, 19	1.9714	0.0000	- - -	2.001 ^b	- - -	1.300
13, 9, 17	2.0939	0.1225	6.21	- - -	- - -	2.250
26, 20, 32	2.2031	0.2317	11.75	2.263 ^b	- - -	3.900

^aExplicit transport-theory calculation.

^bExplicit 2-D transport-theory calculation to obtain g factors for one-dimensional diffusion-theory calculation.

Fig. 7.3--Poison pin interaction study

Fig. 7.4--Worth of three burnable poison pins in central fuel element

Task VIII
PHYSICS AND FUEL MANAGEMENT

LOW-ENRICHMENT CYCLE STUDIES

The study of the low-enrichment-uranium cycle is being conducted in three stages. The first stage assumed the use of the present Fort St. Vrain fuel element with no changes. The second stage uses the same basic fuel element but allows for a more optimum arrangement of fuel and coolant holes. The third stage uses a completely new fuel design with assumed improvements in heat-transfer characteristics. The first two stages of this study have now been completed and are discussed below. Preliminary results of the stage I study were presented in the previous quarterly report (GA-9227). However, a computer code error that significantly affected the fuel cycle costs given in Fig. 8.2 of that report has been detected. The corrected fuel cycle costs for stage I with annual refueling are presented in Fig. 8.1.

A significant factor in the costs associated with the U/Pu cycle is the cost of the uranium, which at low enrichments, is quite sensitive to the enrichment. The required enrichment is strongly dependent on the resonance integral of the U^{238} . The resonance integral, in turn, depends on the degree of self-shielding or lumping that can be obtained. In stage I, the pitch of the fuel rods was fixed at 0.74 in. The resonance integral was dependent on the carbon-to- U^{238} atom ratio which determined the diameter of the fuel rod ($\bar{\lambda}$) and the rod-to-rod (or Dancoff factor, C) shielding. The variation of resonance integral with the parameter $(1 - C)/\bar{\lambda}$ is shown in Fig. 8.2. Indicated on the curve are the resonance integrals associated with some of the stage I calculations.

Stage II allows for the arrangement of the fuel and coolant holes into a more optimum configuration for the low-enrichment-uranium cycle. The prime advantage to be derived with reference to Fig. 8.2 is an increase in the Dancoff factor, which reduces the resonance integral. As a first step in the stage II study, the Dancoff factor was arbitrarily raised to 0.6, which is a probable upper limit consistent with heat-transfer and mechanical-design characteristics. Figure 8.3 compares the initial enrichment of stage I and stage II cases. It is interesting to note that for the lighter cases ($C/U > 500$), stage II enrichments are actually higher than those of stage I. At smaller C/U values, enrichments for stage II are as low as 4%. The increase in enrichment for the lightly loaded cases is due, primarily, to the large drop in conversion ratios (see Fig. 8.4) and the high-burnup requirements of these cases.

Of more interest than the enrichments are the fuel cycle costs (cost parameters were defined in the previous quarterly report, GA-9227). These are shown in Fig. 8.5 for both stages. There is virtually no difference in the minimum fuel cycle costs for the two stages. The optimum, however, has shifted to a considerably heavier loading for stage II. As mentioned previously,

Fig. 8.1--Phase I fuel cycle costs for annual refuel

Fig. 8.2-- U^{238} resonance integral versus $(1 - C)/\bar{l}$ (cm^{-1})

Fig. 8.3--Initial uranium enrichment versus C/U ratio and fuel residence time for semiannual reloads

Fig. 8.4--Conversion ratio versus C/U ratio and fuel residence time

Fig. 8.5--Low-enrichment uranium fuel cycle costs for semiannual reloads with reprocessing

stages I and II are both constrained in temperature levels to values obtained in the Fort. St. Vrain plant. Temperatures can be correlated with the age peaking, and consequently the minimum fuel cycle cost envelope is plotted in Fig. 8.6 versus the age peaking factor. To be consistent with current technology, the age peaking factor must be limited to a value of about 1.4. Thus, the minimum costs are about 1.23 mills/kw-hr.

HTLTR SUPPORT ACTIVITIES

Measurements of k_{∞} for the first HTGR lattice ($C/U^{235} = 6000$, $C/Th = 200$) have been performed up to about 350°C in the BNWL High Temperature Lattice Test Reactor (HTLTR). Preliminary results of these measurements indicate that they are high by about 4%. However, the results have not yet been corrected for cell end effects, which will reduce this discrepancy. These end effects are currently being investigated.

The change in k_{∞} and in the thorium resonance integral (obtained by the vanadium subtraction technique) with temperature appear to be in good agreement with calculated values.

The HTLTR has been heated to 800°C to verify equipment operation. Measurements of k_{∞} up to 800°C (and eventually to 1000°C) will be forthcoming.

Measurements of k_{∞} at room temperature for the first U^{233} lattice ($C/U^{233} = 11,000$, $C/Th = 200$) have been performed in the PCTR (Physical Constants Test Reactor) at BNWL and the results are being analyzed. The use of the PCTR to determine the room temperature k_{∞} will reduce the experimental time required in the HTLTR. In addition, some experimental corrections, such as the cell end effects, are more easily obtained in the PCTR, where there is direct access to the central cell (split bed assembly).

To help evaluate the adequacy of the HTLTR method for determining the k_{∞} of different lattice elements and to determine the sensitivity of these measurements to spatial variations, such as the neutron flux spectrum, the entire HTLTR core has been simulated in an idealized one-dimensional multigroup nuclear model. This model contains five radial regions consisting of the test cell, the remainder of the lattice fuel elements, a buffer region in which the U^{238} loading is adjusted, a core driver region, and the reflector region. In this model, it is assumed that the axial flux is flat, essentially the situation obtained in the test cell region of the HTLTR.

The preliminary analysis for this core assembly was made using 10 broad groups. The 10 broad groups are the same as those used for analysis of the HTGR critical facility experiments and contain four thermal groups, three groups in the resonance energy region, and three fast groups.

The concentration of U^{238} in the buffer and driver regions was allowed to vary until two criteria were satisfied: that the calculated effective multiplication for the assembly be nearly 1.0, and that the cadmium ratio be flat in the region of the HTGR lattice fuel elements. This latter was true for most of the cases considered. A summary of these results is shown in Table 8.1.

Fig. 8.6--Minimum fuel cycle cost versus maximum age peaking for semi-annual reloads

Table 8.1

IDEALIZED HTLTR CORE ASSEMBLY WITH VARIOUS U^{238} LOADINGS IN BUFFER
AND DRIVER REGION AND REFERENCE LATTICE CORE LOADINGS
(C/U = 6000, C/Th = 200)

U^{238} Conc. (10^{-4} atoms/b-cm)		k_{eff} Calc.	Cadmium Ratio for B and Th		
Buffer	Driver		Center of Test Cell	Outer Edge of Test Cell	Outer Edge of HTGR Lattice
2.5	1.6	0.90	0.482 0.366	0.481 0.365	0.489 Boron 0.385 Thorium
2.5	1.19	1.00 ^a	0.494 0.376	0.493 0.375	0.506 0.400
1.9	1.6	0.93	0.486 0.369	0.485 0.368	0.495 0.387
1.9	1.14	1.00 ^a	0.494 0.376	0.493 0.376	0.506 0.397
0.5	1.6	1.01	0.500 0.381	0.498 0.380	0.511 0.395

^aUsed as reference cases for further analysis.

Further analysis will be made for this assembly in which changes are made within the test cell, that is, substitutions of a void, copper, and other lattice elements, and the experimental theory used to predict the k_{∞} of the lattice elements.

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Task IX
ADVANCED COATED PARTICLE FUELS

COATED PARTICLE IRRADIATIONS

Capsule P13L

Capsule P13L is the second test of coated diluent particles. Both carbide and oxide diluent and fertile particles are being tested, with both BISO and TRISO coatings on the diluent particles and BISO coatings on the fertile particles. (Samples of reference TRISO coated fissile and fertile carbide particles are included for comparison.) All diluent samples are of the buffer-diluted design. Details of the sample selection were given in the previous quarterly report (GA-9227). However, a change was made in the list of samples given in Table 9.3 of that report to include a repeat sample from capsule P23 instead of capsule P21. The revised list of samples is given in Table 9.1.

The samples will be irradiated at 1500°, 1300°, and 900°C to design burnups of 75% FIMA for diluent fissile particles, 20% FIMA for conventional fissile particles, and 7% FIMA for fertile particles (full HTGR burnups for all three types of particles). The design fast-neutron dose is $7 \text{ to } 8 \times 10^{21} \text{ n/cm}^2$ (essentially full HTGR exposure). The capsule has begun irradiation in ETR cycle 102.

ORNL Capsule HT-3

ORNL capsule HT-3, which contained four Gulf General Atomic fuel rods together with several ORNL fuel rods, was irradiated for three cycles in HFIR. The uninstrumented capsule was designed to achieve a fast fluence of about $6.5 \times 10^{21} \text{ n/cm}^2$ ($E > 0.18 \text{ Mev}$) at temperatures of about 1050°C (GGA and ORNL samples) and 750°C (ORNL samples only). However, initial results of the flux dosimeter wire analysis revealed several inconsistencies, and analysis of the SiC temperature monitor strips that were included only in the low-temperature cell indicated that this cell operated at about 660°C instead of the design 750°C.

The Gulf General Atomic fuel rods included two fuel rods prepared by the injection-molding process and two by the overcoating process. Each set contained one rod with TRISO LTI coated ZrO_2 particles and one with TRISO HTI coated ZrO_2 .

Postirradiation visual examination at ORNL revealed that all four of the Gulf General Atomic fuel rods were completely debonded. (All of the ORNL rods irradiated at high temperature also appeared to be completely debonded, although many of the ORNL rods irradiated at low temperature were still intact.) Because of the almost complete debonding of the fuel, some spillage and cross-mixing of the coated particles from the four Gulf General Atomic rods occurred.

Table 9.1
COATED PARTICLES TO BE IRRADIATED IN CAPSULE P13L

Batch No.	Material Type	Core Type	Coating Class	Overcoat Type ^a	Planned Irradiation Temperature (°C)	Test Objectives
4413-35E	Fissile	UO ₂	BISO	LTI	900,1300,1500	Oxide series
4403-113E	Fissile	UC ₂	BISO	LTI	900,1300,1500	Conservative design
4413-5E	Fissile	UC ₂	TRISO	LTI	900,1300,1500	Conservative design
4413-67E	Fissile	UO ₂	TRISO	LTI	900,1300,1500	Oxide series
3516-47E	Fissile	UC ₂	BISO	HTI	900,1300,1500	Conservative design
4263-75E	Fissile	(Th,U)C ₂	TRISO	LTI	900,1500	P23 repeat
4413-75	Fertile	ThO ₂	BISO	LTI	700	Fertile oxide series
4423-89	Fertile	ThC ₂	BISO	LTI	700	Fertile carbide series
4403-81E	Fissile	UC ₂	BISO	LTI	1300	Conservative design
3516-33E	Fissile	UC ₂	TRISO	HTI	1300	Conservative design
4403-143E	Fissile	UC ₂	TRISO	LTI	1300	Conservative design
3516-39E	Fissile	UC ₂	TRISO	HTI	1300	Conservative design
4413-21	Fissile	UC ₂	TRISO	LTI	1300	Fail-early design
4423-67	Fertile	ThC ₂	TRISO	LTI	700	Fertile carbide series

^a LTI refers to the type of coating that is derived from propane at deposition temperatures less than 1600°C; HTI coatings are derived from methane at temperatures greater than 1600°C.

Metallographic examination of the material from each rod and mixed debonded particles indicated little evidence of conventional irradiation damage to the particle coatings in the injection-molded rods. However, severe corrosion of the outer PyC and, in some cases, the SiC layers was observed in many particles. The cause of this corrosion has not been determined; it may have been caused by either the ZrO_2 kernels or by Al_2O_3 disks located adjacent to the fuel rods. (Corrosion was also observed on some of the ORNL coated particles.)

The overcoated rods had several particles with broken outer PyC coatings. None had damaged SiC or inner PyC layers, and there was no evidence of corrosion. The broken pieces of the outer PyC coating appeared to have been pulled loose by adhering matrix. A high concentration of binder coke was visible at the interface between the PyC and matrix in several of the regions where the outer PyC had pulled away from the underlying SiC, and this high binder content may have led to a bond between the matrix and PyC that was strong enough to permit the shrinking matrix to pull the PyC away from the SiC. (The overcoated rods had a higher binder content than the injection-molded rods.)

The initial results of the HT-3 flux dosimeter analyses indicated that (1) results of the stainless steel wire and nickel wire dosimeters disagreed with each other, (2) measured fluences were less than the calculated fluences, and (3) measured fluences were lower than measured fluences in the earlier HT-1 capsule which was irradiated in a comparable HFIR core position for a similar time and which agreed well with calculated fluences. To resolve these discrepancies, an investigation was conducted to evaluate dosimetry techniques. The results of the Gulf General Atomic studies indicated that the actual fast fluence could only be bracketed between the measured fluences (normalized between HT-3 and HT-1) as the lower bound and the calculated fluences (based upon transport calculations and several independent flux measurements in HFIR) as the upper bound, and could not be defined with greater accuracy. The data thus indicated that the peak fast fluence in HT-3 lay between 5×10^{21} and 7.6×10^{21} n/cm² ($E > 0.18$ Mev). (This is estimated to be equivalent to 4 to 6.5×10^{21} n/cm² ($E > 0.18$ Mev) in terms of relative exposure in an HTGR.)

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Task XI
GRAPHITE RESEARCH

IRRADIATION EFFECTS IN CARBONS AND GRAPHITE

Irradiation-induced Dimensional Changes in HTI Carbons

Results from the third irradiation of carbon specimens in capsule GE-H-13-15 and those from the P13J and P13K capsule specimens have significantly extended the data that relate the dimensional behavior of turbostratic carbons to their structure. The data are now sufficiently extensive to warrant an interim summary, together with a discussion of the implications of the results to the over-all understanding of irradiation effects in carbons and graphite. Essential background material has been recently summarized in Ref. 1; accordingly, only a brief introduction is presented here.

When polycrystalline carbons and graphites are irradiated with fast neutrons, carbon atoms are displaced from their positions in the lattice to produce a homogeneous dispersion of interstitials and vacancies. Depending on the temperature, the defects interact and diffuse to various crystal defects, where they are annihilated. Many of the interstitials end up in new layer planes that are nucleated between the original layers. The net result is that the individual crystallite tends to grow in the c direction and contract in the a direction (Fig. 11.1). Because the rates of rearrangements of the defects that are generated by the irradiation are determined by diffusion and because the annihilation sites are predominantly crystal defects, the temperature dependence of the shape changes is complex and the changes are strongly structure sensitive (Ref. 2). The main obstacle to the development of a useful quantitative model based on simple atomistic processes to describe the dimensional behavior of practical carbonaceous materials (i.e., anything other than a single crystal) has been the inability to accurately characterize the crystal structure of quasi-crystalline carbons and graphites. Hence, most attempts at developing a model have been based on phenomenology (Refs. 3, 4).

It has been shown that the dimensional changes of bulk samples of turbostratic carbon can be related to the individual crystallite changes if one uses the simple crystallite averaging procedures that were first applied by Bacon (Ref. 5) to describe the thermal expansion behavior of polycrystalline graphite and later used by others (see Refs. 6, 7, 8) to interpret bulk dimensional changes induced in carbons and graphites by fast-neutron irradiation. In this approach, the strain in a bulk dimension $\ln[1 + (\Delta\ell_i/\ell_i)]$ is related to the principal crystallite strains in the c and a directions, $\ln[1 + (\Delta X_c/X_c)]$ and $\ln[1 + (\Delta X_a/X_a)]$, by a preferred orientation parameter R that is determined by X-ray diffraction (Ref. 9):

$$\ln \left(1 + \frac{\Delta\ell_i}{\ell_i} \right) = R_i \ln \left(1 + \frac{\Delta X_a}{X_a} \right) + (1 - R_i) \ln \left(1 + \frac{\Delta X_c}{X_c} \right) \quad (1)$$

Fig. 11.1--Schematic diagram illustrating the dimensional changes induced in a carbon crystallite by fast-neutron irradiation: (a) before irradiation, (b) after irradiation (LC-33620)

Since turbostratic pyrolytic carbons are crystallographically symmetrical about the normal to the deposition plane, each deposit is characterized by two R values, R_{pa} (parallel) and R_{pe} (perpendicular) (Ref. 6). The two R parameters are related by

$$R_{pa} = 1 - (R_{pe}/2) \quad . \quad (2)$$

R_{pe} is related to the Bacon anisotropy factor (BAF) by

$$R_{pe} = \frac{2}{2 + \text{BAF}} \quad . \quad (3)$$

All the values of $\ln[1 + (\Delta l_{pa}/l_{pa})]$ and $\ln[1 + (\Delta l_{pe}/l_{pe})]$ that are now available for turbostratic carbons irradiated at 900°C are plotted in Fig. 11.2 as a function of the original R_{pa} and R_{pe} values. The data are for dense ($\rho_0 > 2.1 \text{ g/cm}^3$) turbostratic carbons with L_c between 50 and 200 Å (HTI carbons). At low fluences (less than $3 \times 10^{21} \text{ n/cm}^2$, $E > 0.18 \text{ Mev}$), the data for all the carbons fall on a single straight line, which means that the crystallites in all the carbons have changed shape by the same amount and that the crystallite strain is independent of the anisotropy of the deposits. There are no significant deviations resulting from crystallite interactions such as are observed for similar plots relating thermal expansivity to preferred orientation (Ref. 7). The principal crystallite changes can be obtained by extrapolating the linear plots to $R_{pe} = 0$ and $R_{pa} = 1$.

For fluences greater than $3.0 \times 10^{21} \text{ n/cm}^2$, the data for all the carbons at a given fluence do not fall on a single straight line, but rather describe a sigmoidal-shape curve. Measurements of the degree of preferred orientation of specimens irradiated to $5.6 \times 10^{21} \text{ n/cm}^2$ at about 1000°C have shown that the sigmoidal shape is not due to changes in preferred orientation that are caused by the irradiation (Ref. 10). Since the sigmoidal shape is not due to changes in anisotropy, the explanation probably is that the rate at which the crystallites change shape accelerates because of the irradiation, and the effect is most pronounced for the isotropic carbons (i.e., those with R_{pe} and R_{pa} nearest 2/3). Since it is well known that the rate at which the crystallites change dimensions during irradiation at 900°C is most rapid for the most poorly crystalline carbons, and since the degradation of the crystalline structure is expected to be most rapid for dense isotropic carbons in which crystallite interactions are a maximum, it is suggested that the sigmoidal-shaped curves in Fig. 11.2 develop as a result of an autocatalytic increase in the rate at which the crystallites in the dense, most isotropic deposits change shape. The increase is taken to be due to the structural degradation of these carbons during irradiation.

The rate for an irradiation temperature of 900°C at which the crystallites in a dense, nearly isotropic ($\text{BAF} = 1.2$) carbon change dimension are compared in Fig. 11.3 with the corresponding rate for the crystallites in an anisotropic ($\text{BAF} = 2.4$) carbon. The data in Fig. 11.3 were derived from those in Fig. 11.2 by extrapolating the data for each exposure to $R_{pe} = 0$ and $R_{pa} = 1$ for the cases $\text{BAF} = 1.2$ and 2.4. The curves in Fig. 11.3 show that the c direction growth rate is nearly constant for the anisotropic carbon deposite, but accelerates

Fig. 11.2--Plots of $\ln[1 + (\Delta l_i/l_i)]$ versus the preferred orientation parameter R_i for HTI carbons ($50 \text{ \AA} < L_c < 200 \text{ \AA}$) with $\rho_0 > 2.1 \text{ g/cm}^3$ irradiated at 900°C (LC-70330)

Fig. 11.3--Crystallite c-direction growth $\ln[1 + (\Delta X_c/X_c)]$ versus neutron exposure ($E > 0.18 \text{ Mev}$) for dense ($\rho_0 > 2.1 \text{ g/cm}^3$) HTI carbons irradiated at 900°C (LC-71808)

rapidly with exposure for the nearly isotropic carbon. At an exposure between 4 and 5×10^{21} n/cm², the rate is about 0.8 per 10^{21} n/cm², which is nearly equal to the maximum rate possible for the case in which every displaced atom ends up in a position in a new layer plane that was formed during the irradiation (Refs. 11, 12).

Further insight into the damage to carbon caused by neutron irradiation at 900°C is obtained from the dimensional changes measured for nearly isotropic ($\text{BAF} < 1.1$) carbon. Of particular interest is the effect of the original density of the deposit on its dimensional behavior. Just as the acceleration effect noted for dense carbons approached a maximum as the BAF approached 1.0 , it might be expected that the acceleration effect for isotropic carbon would be maximum for the most dense deposits. This is because internal porosity can provide internal accommodation of crystallite strains (Ref. 7), which means that crystallite interactions must be most severe in dense deposits with a minimum of porosity.

The crystallite c-direction growth rate for dense anisotropic deposits during irradiation to low fluences at 900°C is about 0.2 per 10^{21} n/cm² (Ref. 2). Hence, when the plot in Fig. 11.4 is used, the maximum rate of dimensional change for isotropic ($\text{BAF} < 1.1$) carbon can be estimated. Since the uncertainty in measured values of the BAF may be as much as 0.05 , a value of 1.15 is used as the maximum possible BAF for carbons with measured values of BAF less than 1.1 . The plot shows that the perpendicular growth rate of a dense carbon with $\text{BAF} = 1.15$ is 0.01 per 10^{21} n/cm². Without acceleration, strains of about 0.1 are expected for an exposure of 10^{22} n/cm². If there is an acceleration effect and no change in anisotropy due to irradiation, the maximum theoretical limit for the perpendicular growth rate can be estimated from the maximum theoretical limit for the c-axis growth rate, i.e., 1 per 10^{21} n/cm² (Refs. 11, 12). The value for a dense, nearly isotropic carbon with $\text{BAF} = 1.15$ is 0.05 per 10^{21} n/cm², or 0.5 per 10^{21} n/cm².

The dimensional change data for isotropic ($\text{BAF} < 1.1$) carbons irradiated at 900° to 1000°C are plotted in Fig. 11.5 as a function of their preirradiation density. The uppermost curve for each exposure is for the perpendicular direction; the lower curve is for the parallel direction. The data show that all the carbons shrink isotropically at low exposures and the shrinkage rate is maximum for the carbons with the lowest density. At an exposure of about 3×10^{21} n/cm², all the carbons start to behave anisotropically; the effect is most pronounced for the densest carbons. At higher exposures, the anisotropy of the dimensional changes is more pronounced, and the dependence on density becomes strong for carbons with densities above about 1.9 g/cm³. At an exposure of 5.6×10^{21} n/cm², the perpendicular growth for the most dense carbon is about 0.25 . For no change in preferred orientation and no acceleration of the crystallite dimensional change rate, the curve for low fluences at 900°C in Fig. 11.4 predicts a bulk dimensional change of only 0.05 at an exposure of 5×10^{21} n/cm². Since postirradiation measurements have shown no significant change of preferred orientation for isotropic specimens irradiated to 5.6×10^{21} n/cm² at 1000°C , the conclusion is the same as that reached for the anisotropic deposits discussed above, i.e., at 900° to 1000°C , there is an acceleration in the crystallite dimensional change rate and the effect is most pronounced for the most dense, isotropic carbons.

Fig. 11.4--Plot relating bulk dimensional changes $\ln[1 + (\Delta \ell_i / \ell_i)]$ to the crystallite dimensional changes for dense, isotropic (BAF < 1.1) HTI carbons irradiated at 900° and at 1250°C (LC-71809)

Fig. 11.5--Bulk dimensional changes $\ln[1 + (\Delta \ell / \ell)]$ plotted as a function of preirradiation density for isotropic (BAF < 1.1) carbons with L_c between 50 Å and 200 Å irradiated at 900°C (left) and at 1000°C (right). The uppermost curve at each dose is for the perpendicular direction; the lower curve is for the parallel direction. For easy comparison, the curve for 4.6×10^{21} n/cm² (at 900°C) has been included on both plots (LC-70335)

The dimensional changes for the specimens in Fig. 11.5 irradiated to 6.0×10^{21} n/cm² and above are in excess of those predicted from the calculated maximum theoretical rate. This must mean that at exposures above about 6×10^{21} n/cm² at 900° to 1000°C, changes in preferred orientation must be caused by the irradiation. Postirradiation preferred orientation measurements on specimens irradiated to fluences in excess of 6×10^{21} n/cm² at 900° to 1000°C have not been reported. In order to explain the measured changes plotted in Fig. 11.5 for the most dense carbons irradiated to 6.0×10^{21} to 6.6×10^{21} n/cm², the BAF would have had to increase to at least 1.3.

In summary, it may be concluded that the crystal structure of turbostratic carbon ($50 \text{ \AA} < L_c < 200 \text{ \AA}$) irradiated at a temperature near 900°C is changed by the irradiation, and that, for the most dense, isotropic carbons, the changes cause an acceleration in the crystallite dimensional change rate. It appears that these changes are due primarily to an irradiation-induced degradation of the crystal structure, which leads to increasing dimensional instability. At exposures above about 6×10^{21} n/cm², irradiation-induced increases in preferred orientation are suspected. These ideas are illustrated schematically in Fig. 11.6; the data relating the volume expansion associated with porosity generation during the irradiation to the neutron exposure are plotted in Fig. 11.7.

Although many features of the dimensional behavior of turbostratic carbon irradiated at 1250°C are similar to those observed for specimens irradiated at 900°C, there are several important differences. At 1250°C, the rate at which the crystallites change dimensions and the rate of initial densification are higher than they are at 900°C (see Fig. 11.8). Comparison of the data in Figs. 11.7 and 11.8 shows that at 1250°C the maxima in the individual curves are greater than the corresponding maxima reached at 900°C, and, at high exposures, the magnitude of the expansions at 900°C are greater than they are at 1250°C. The data in Fig. 11.9 emphasize the latter point.

Another difference between the effects of irradiation at 900° and at 1200°C is brought out by data shown in Fig. 11.10, which compares the changes in the apparent crystallite height L_c for carbons with the same initial structure that were irradiated at 900° and at 1200°C. The data show that the L_c parameter is reduced much more during irradiation at 900°C than it is at 1200°C. The fact that the crystal structure is degraded less rapidly at 1200°C than at 900°C, in spite of the fact that the rate at which the crystallite changes shape is much higher at 1200°C than it is at 900°C, has been attributed to more rapid and effective intercrystalline irradiation creep at the higher temperature (Ref. 1). The same rationalization is offered to explain the more rapid volume expansion at 900° than at 1200°C (see Fig. 11.9).

The bulk dimensional changes $\ln[1 + (\Delta l_{pe}/l_{pe})]$ and $\ln[1 + (\Delta l_{pa}/l_{pa})]$ for turbostratic carbons irradiated at 1250°C are plotted in Fig. 11.11 as a function of the original R_{pe} and R_{pa} values. The data are for dense ($\rho_0 > 2.1 \text{ g/cm}^3$) turbostratic carbons ($50 \text{ \AA} < L_c < 200 \text{ \AA}$). The responses to irradiation are similar to those plotted in Fig. 11.2 for irradiations at 900°C. For exposures above about 2×10^{21} n/cm², the data points described a sigmoidally-shaped curve. Analysis of the data in Fig. 11.11 using the data for a highly oriented carbon ($BAF = 2.4$, $R_{pe} = 0.45$, $R_{pa} = 0.775$) and a nearly

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Fig. 11.6--Schematic diagram illustrating irradiation-induced expansion and structural degradation of initially dense, isotropic turbostratic carbon irradiated at about 900°C, where irradiation creep between crystallites is not very effective (LC-71956)

Fig. 11.7--Density of turbostratic carbons ($50 \text{ \AA} < L_c < 200 \text{ \AA}$) versus neutron exposure for irradiations at 900° to 1000°C (LC-70852)

Fig. 11.8--Density of turbostratic carbons ($50 \text{ \AA} < L_c < 200 \text{ \AA}$) versus neutron exposure for irradiations at 1250°C (LC-70853)

Fig. 11.9--Density of a turbostratic carbon ($L_c = 145 \text{ \AA}$) versus neutron exposure for irradiations at 900° and at 800°C followed by irradiations at either 1000° or at 1280°C (LC-72073)

Fig. 11.10---Change in L_c due to irradiation at 900° and at 1200°C versus the original L_c parameter (LC-72007)

Fig. 11.11--Bulk dimensional change $\ln[1 + (\Delta l/l)]$ versus R_i for dense ($\rho_0 > 2.1 \text{ g/cm}^3$) HTI carbons ($50 \text{ \AA} < L_c < 200 \text{ \AA}$) irradiated at 1250°C (LC-70331)

isotropic carbon ($\text{BAF} = 1.33$, $R_{pe} = 0.60$, $R_{pa} = 0.70$) yields the crystallite growth $\ln[1 + (\Delta X_c/X_c)]$ as a function of neutron exposure for the two structures (see Fig. 11.12). These data are similar to those derived for carbons irradiated at 900°C (see Fig. 11.3), except that crystallite growth rates for both carbons irradiated at 1250°C are greater. It should be noted specifically that the growth rate calculated for the carbon with an initial BAF of 2.4 is almost twice that calculated to be theoretically possible. An obvious explanation is that important changes in preferred orientation occur during irradiation at temperatures above 1000°C (Ref. 13).

The bulk dimensional changes for turbostratic HTI ($\text{BAF} < 1.1$) carbon irradiated at 1250°C are plotted in Fig. 11.13 as a function of their original density. The extrapolations in Fig. 11.4 show that if changes in anisotropy do not occur and the crystallite growth rate is constant and equal to 0.5 per 10^{21} n/cm^2 , then the maximum bulk change $\ln[1 + (\Delta \ell/\ell)]$ in the perpendicular direction would be 0.5 for the samples irradiated to 9.3 to $10.3 \times 10^{21} \text{ n/cm}^2$. The actual change was 1.0. These differences may be attributable to accelerations in the crystallite growth rate due both to degradation of the crystal structure caused by irradiation and irradiation-induced increases in preferred orientation. The two effects cannot be separated until the postirradiation anisotropy of the specimens has been measured. These measurements are in progress.

The schematic diagram in Fig. 11.14 provides a summary of the rationalization of the dimensional changes that occur in HTI carbons at 1250°C . At 1250°C , effects similar to those that occur at 900°C are observed. The crystal structure is degraded by the irradiation at 1250°C , but to a lesser extent than what occurs at 900°C . Changes in preferred orientation are more pronounced at 1250° than at 900°C . Both effects are likely to be manifestations of the fact that irradiation creep is more effective in relieving internally generated stress between crystallites at 1250° than at 900°C . Because crystallite interactions must be most intense for dense, isotropic deposits, the accelerations in bulk growth rates due both to increases in the rate at which the crystallites change shape and to increases in anisotropy are most pronounced for these carbons.

The changes in thermal conductivity of HTI carbon caused by irradiation at 900° and at 1250°C that are shown in Fig. 11.15 are consistent with the above rationalization. After irradiation, the thermal conductivity of specimens irradiated at 900°C are all lower by at least a factor of two than those for specimens irradiated at 1200°C , and the lowest postirradiation conductivities are for those carbons with the highest preirradiation density. More extensive thermal conductivity data for irradiated HTI carbons can be found in Ref. 14.

For engineering purposes, it is most convenient to have data such as those in Figs. 11.5 and 11.13 plotted to show the dimensional changes as a function of exposure. Accordingly, the data in Figs. 11.5 and 11.13, together with data for irradiations at 600° and 1100°C given in Figs. 11.16 and 11.17, have been plotted (see Figs. 11.18 through 11.20) to illustrate the neutron-exposure dependence of the dimensional changes induced in isotropic ($\text{BAF} < 1.1$) HTI carbons for irradiation temperatures of 600° , 900° , 1100° , and 1250°C .

Fig. 11.12--Crystallite c-direction growth $\ln[1 + (\Delta X_c/X_c)]$ versus neutron exposure ($E > 0.18 \text{ Mev}$) for dense ($\rho_0 > 2.1 \text{ g/cm}^3$) carbons irradiated at 1250°C (LC-71807)

Fig. 11.13--Bulk dimensional changes $\ln[1 + (\Delta l/l)]$ versus the preirradiation density for isotropic (BAF < 1.1) HTI carbons irradiated at 1250°C. The open points are for the perpendicular direction and the solid points are for the parallel direction (LC-70329)

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Fig. 11.14--Schematic diagram illustrating irradiation-induced expansion, structural degradation, and increases in preferred orientation for initially dense, nearly isotropic turbostratic carbons irradiated at 1250°C, where irradiation creep between crystallites is effective (LC-71957)

Fig. 11.15--Thermal conductivity at 500°C for unirradiated HTI carbons and for carbons irradiated at 900° and 1200°C, plotted as a function of the preirradiation density (LC-72005)

Fig. 11.16--Bulk dimensional change versus preirradiation density for isotropic (BAF < 1.1) HTI carbons irradiated at 600°C. Open points are for perpendicular directions; solid points are for parallel directions (LC-71803)

Fig. 11.17--Bulk dimensional change versus preirradiation density for isotropic (BAF < 1.1) HTI carbons irradiated at 1100°C. Open points are for perpendicular directions; solid points are for parallel directions (LC-72076)

Fig. 11.18--Bulk dimensional change versus neutron exposure ($E > 0.18 \text{ Mev}$) for isotropic (BAF < 1.1) HTI carbons ($50 \text{ \AA} < L_c < 200 \text{ \AA}$) with preirradiation density of 1.55 g/cm^3 , irradiated at 600° , 900° , 1100° , and 1250°C (LC-72013)

Fig. 11.19--Bulk dimensional change versus neutron exposure ($E > 0.18 \text{ Mev}$) for isotropic (BAF < 1.1) HTI carbons ($50 \text{ \AA} < L_c < 200 \text{ \AA}$) with preirradiation density of 1.70 g/cm^3 , irradiated at 600°, 900°, 1100°, and 1250°C (LC-72010)

Fig. 11.20--Bulk dimensional change versus neutron exposure ($E > 0.18 \text{ Mev}$) for isotropic (BAF < 1.1) HTI carbons ($50 \text{ \AA} < L_c < 200 \text{ \AA}$) with preirradiation density of 1.90 g/cm^3 , irradiated at 600° , 900° , 1100° , and 1250°C (LC-72011)

The data in Figs. 11.18, 11.19, and 11.20 are for preirradiation densities of 1.55, 1.70, and 1.90 g/cm³, respectively. Similar plots for slightly anisotropic (BAF = 1.3) carbon with a preirradiation density of 2.0 g/cm³ are presented in Fig. 11.21 and for a very anisotropic (BAF = 2.2) carbon with a preirradiation density of 2.1 g/cm³ in Fig. 11.22.

The data in Figs. 11.18-11.20 contrast the weak temperature dependence of the shrinkage due to densification at low exposures with the strong temperature dependence of the shape change at high exposures. Of particular interest is the fact that the lowest-density ($\rho_0 = 1.55$ g/cm³) isotropic carbons offer the best over-all dimensional stability. To survive as a coating on a relatively unyielding support such as an overcoat on a SiC layer, however, a carbon with an initial density of 1.5 g/cm³ must endure high shrinkage rates early in life. The shrinkage rate for the carbon with $\rho_0 = 1.55$ g/cm³ is about 6% per 10²¹ n/cm² at 1250°C. Irradiation creep data reported in Ref. 1 indicate that at 1250°C a shrinkage rate of 6% per 10²¹ n/cm² is just tolerable without fracture. In a recent test of coated nuclear fuel particles (capsule P23), in which coatings on particles with and without underlying SiC layers were irradiated to about 2×10^{21} n/cm² at 1200° to 1300°C, coatings with initial densities of 1.55 g/cm³ survived. Since a lower limit of 10% for the total creep strains has been established at 1250°C, these results suggest that low-density carbon coatings should survive to at least 6×10^{21} n/cm² at 1250°C when irradiated as an overcoat on SiC substrates and to higher doses when irradiated as an overcoat on low-density buffer coatings such as on a BISO particle. The ability to use low-density isotropic carbons results in substantial fabrication economies.

Only a meager amount of creep data have been accumulated since the data given in Ref. 1 were published. Additional data points at an exposure of 3.2×10^{21} n/cm² have been added to the data for irradiation at 900°C (Fig. 11.23). The envelope of the data points in Fig. 11.23 approximates the creep curve at the fracture stress. These data, together with those in Ref. 1, show that creep strains of 2%, 3%, and 6% per 10²¹ n/cm² are tolerable at 600°, 900°, and 1200°C, respectively, and that total creep strains of at least 10% are possible at 900° and 1200°C.

One of the questions that remains to be answered is the influence of prior irradiations at one temperature on subsequent irradiation at another temperature. For turbostratic carbons with L_c between 120 and 200 Å, data in Ref. 10 show that an irradiation to a low exposure at about 600°C has no influence on subsequent irradiation behavior at 900°C, i.e., there is no seeding effect. Other data in Ref. 1 indicate that irradiations to substantial exposures at high temperature have a pronounced effect on irradiations at lower temperatures. It was shown that a prior irradiation at 1650°C was much more damaging than an irradiation at 950°C to the same exposure. Difficulties arise, however, when an attempt is made to establish an equivalent between the damage accumulated at high irradiation temperatures and that accumulated at low temperatures, because states of damage can be attained during irradiation at high temperature that are never achieved in the same carbon during irradiation at a lower temperature.

Fig. 11.21--Bulk dimensional changes versus neutron exposure ($E > 0.18 \text{ Mev}$) for anisotropic carbon with $50 \text{ \AA} < L_c < 200 \text{ \AA}$ irradiated at 600° , 900° , 1100° , and 1250°C ; for $\text{BAF} = 1.3$ and $\rho_0 = 2.0 \text{ g/cm}^3$ (LC-72012)

Fig. 11.22--Bulk dimensional changes versus neutron exposure ($E > 0.18 \text{ Mev}$) for anisotropic carbon with $50 \text{ \AA} < L_c < 200 \text{ \AA}$ irradiated at 600° , 900° , 1100° , and 1250°C ; for $\text{BAF} = 2.2$ and $\rho_0 = 2.1 \text{ g/cm}^3$ (LC-71811)

Fig. 11.23--Creep strain versus neutron exposure for irradiations at 900°C
(LC-72006)

Data illustrating the effect of an irradiation at 800° to 900°C on subsequent dimensional behavior during irradiation at 1280°C are presented in Figs. 11.9 and 11.24. For this case (a temperature increase during irradiation), the data suggest that it may be possible to establish an equivalent-damage rule. If the data in Fig. 11.9 for the irradiation at 1280°C are translated horizontally to lower exposures and are fitted on the curve on Fig. 11.8 for $\rho_0 = 2.0 \text{ g/cm}^3$, then the position of the segment of the curve on Fig. 11.9 for the 1280°C irradiation agrees reasonably well with the corresponding data on Fig. 11.8. Similarly, if the curves on Fig. 11.24 for the segment of the irradiation at 1280°C are translated horizontally to lower exposures until the points initially at $4.4 \times 10^{21} \text{ n/cm}^2$ fall on the corresponding curves in Fig. 11.21 for the carbon with the same initial structure, then the positions of the segments of the curve on Fig. 11.24 for the 1280°C irradiation agree reasonably well with corresponding data on Fig. 11.21. Although the procedure works for this particular case, more data will be needed to determine whether the correspondence is merely fortuitous. Work is in progress to obtain more data relating the effect of irradiation damage accumulated at one temperature to later behavior during irradiation at another temperature.

Irradiation-induced Dimensional Changes in LTI Carbons

Although it has been known for several years that the dimensional stability of carbons during high-temperature neutron irradiation depends strongly on their crystalline perfection (Refs. 2, 8), there is still only a limited amount of data that describe the dimensional behavior of the poorly crystalline ($L_c < 50 \text{ Å}$) family of isotropic carbons that are deposited at temperatures below about 1500°C (LTI carbons). This is because early efforts were aimed primarily at developing more stable HTI coatings. Because of certain fabrication economies that are realized when carbon is deposited at temperatures below about 1500°C, efforts have been directed toward understanding the irradiation behavior of LTI carbon. Many irradiation experiments designed to yield the required data for LTI carbons are still in progress. It is possible with the limited data available, however, to make a comparison between the stability of LTI and HTI carbons during neutron irradiation.

The plots of density versus neutron exposure for LTI carbons in Fig. 11.25 can be compared with corresponding data for HTI carbons that are plotted in Figs. 11.7, 11.26, and 11.27. Note that the carbon with an initial density of 1.55 g/cm^3 and $L_c = 50 \text{ Å}$ has been arbitrarily selected to straddle the boundary between LTI and HTI carbons and, accordingly, has been plotted with the data for both these types of carbons. A comparison of these data shows that the initial densification of LTI carbons is substantially faster than that of HTI carbon.

Data relating the lineal bulk dimensional changes of LTI and HTI carbons are plotted in Fig. 11.28. The data suggest further that LTI carbons are not as dimensionally stable as HTI carbons during irradiation, but the data are too limited for a quantitative discussion.

Measurements of irradiation creep have shown that creep strains in excess of 3% can be tolerated at 1000°C, but these data consist of only several points, and a statement regarding a limiting creep strain is not yet possible. A

Fig. 11.24--Bulk dimensional change for a carbon with $L_0 = 145 \text{ \AA}$, BAF = 1.3, and $\rho_0 = 2.0 \text{ g/cm}^3$ irradiated at 900°, 800°, and then at either 1000° or 1280°C (LC-72072)

Fig. 11.25--Density versus neutron exposure ($E > 0.18$ Mev) for LTI carbons irradiated at 600°C (LC-72008)

Fig. 11.26--Density versus neutron exposure ($E > 0.18$ Mev) for LTI carbons irradiated at 1000°C (LC-72009)

Fig. 11.27--Density versus neutron exposure ($E > 0.18$ Mev) for HTI carbons irradiated at 600°C (LC-65006)

Fig. 11.28--Plots of bulk dimensional changes $\ln[1 + (\Delta l/l)]$ versus the preirradiation density for LTI carbons irradiated (left) at 600°C and (right) at 1000°C (LC-72074)

number of experiments that are in progress will remove much of the uncertainty regarding the stability of LTI carbons. It should be kept in mind that although LTI carbons are less stable than HTI carbons (at least at low fluences), they are also much stronger. Hence, LTI carbons may prove to be superior to HTI carbon for use as coatings for fuel particles.

Dependence of Dimensional Stability of Carbon and Graphite on Crystalline Perfection

The reactor cores of HTGRs are constructed of a variety of carbonaceous materials, the crystal structures and morphologies of which vary widely. Accordingly, studies have been carried out on the relationship between crystalline perfection and dimensional behavior.

Lineal bulk dimensional changes for dense materials with high degrees of preferred orientation are plotted in Figs. 11.29 and 11.30. The data are for natural graphite crystals, annealed pyrolytic graphite, catalytically graphitized carbon, and turbostratic carbon. Structural parameters characterizing each material are listed in Fig. 11.29. For the natural crystals, the data give the crystallite changes directly. Since the other materials are not perfectly oriented, the crystallite changes must be obtained by a Bacon extrapolation. At low exposures, where changes in preferred orientation and the accelerating effects of accumulated irradiation damage are not pronounced, one can obtain an estimate of the crystallite changes by multiplying the lineal changes in Figs. 11.29 and 11.30 for annealed pyrolytic, catalytically graphitized, and the turbostratic carbon by 1.2, 3.1, and 3.5, respectively. Note that because of the relations shown in Fig. 11.3, the crystallite changes obtained from the high-fluence data are peculiar only to these particular materials and cannot be used generally to determine the bulk changes of carbonaceous materials with different morphologies (i.e., different internal restraints) or different degrees of preferred orientation.

In any case, the data demonstrate the strong dependence at high irradiation temperature of neutron-induced dimensional changes on the initial crystalline perfection of carbonaceous materials. At 600°C (see Fig. 11.31), the data for all the materials fall together, and the dependence on crystalline perfection is minimal.

CONTROL ROD MATERIALS IRRADIATION STUDIES

Capsule BG-1

The postirradiation examination of the specimens irradiated in capsule BG-1 was completed, and the detailed results and conclusions will be published in Ref. 15. The results and conclusions for that experiment are summarized briefly here.

Boronated graphites containing 21 to 35 wt-% boron and fabricated by extrusion or warm-pressing techniques were irradiated at $650 \pm 100^\circ\text{C}$ to fast-neutron exposures of 2 to 4×10^{21} n/cm² ($E > 0.18$ Mev), which were similar to two to four years exposure in an HTGR. After irradiation, the specimens were unchanged in visual appearance from the unirradiated condition. Figure

Fig. 11.29--Bulk lineal dimensional changes versus neutron exposure ($E > 0.18 \text{ Mev}$) for carbonaceous materials with structures that vary widely in crystalline perfection; irradiated at 1250°C (LC-71802)

Fig. 11.30--Bulk lineal dimensional changes versus neutron exposure ($E > 0.18$ Mev) for carbonaceous materials with structures that vary widely in crystalline perfection; irradiated at 900°C (data for the natural crystals irradiated at 900°C extend to 3×10^{21} n/cm² and are coincident with those for annealed pyrolytic graphite) (LC-71810)

Fig. 11.31--Bulk lineal dimensional changes versus neutron exposure ($E > 0.18 \text{ Mev}$) for carbonaceous materials with structures that vary widely in crystalline perfection; irradiated at 600°C (at 600°C , the data for all materials fall in the indicated bands) (LC-71806)

11.32 shows several of the material types after irradiation. The specimens were shielded from thermal neutrons so that a B^{10} burnup gradient (35% to 80%) developed across a 0.2-in. width of some specimens, as shown in Fig. 11.33. Dimensional and physical property measurements before and after irradiation are shown in Table 11.1. The irradiation caused an increase in the coefficient of thermal expansion and a decrease in thermal conductivity. The flexure strength was unchanged. Dimensional change of boronated graphites during irradiation was the most important determination of the experiment.

The observed dimensional change of the extruded boronated graphite (material 3912-34) perpendicular and parallel to the extrusion direction is shown in Fig. 11.34 as a function of fast-neutron exposure. Other reported values (Refs. 16-18) for irradiation-induced dimensional change in extruded boronated graphite are also shown for comparative purposes. There is a lack of agreement in data. The boron content, neutron energy spectrum, and irradiation temperature differed for each source of data in Fig. 11.34, and these differences probably contributed to the lack of agreement between experimental results of various workers.

Experimental error must also contribute to the lack of agreement with respect to dimensional change. The percentage changes shown in Fig. 11.34 represent for the most part measured changes of 0.005 in. or less. This is about the lower limit of detectability because of the coarse texture of the measured surface. In spite of the uncertainties involved, the data on extruded boronated graphite show that dimensional change will be relatively small under conditions of irradiation experiments conducted to date.

In the warm-pressed and baked material, the irradiation-induced dimensional change was not dependent on specimen size or B^{10} burnup gradient. A comparison of dimensional changes exhibited by shielded, nonshielded, and full-sized specimens of two boronated graphites is shown in Fig. 11.35. The B^{10} burnup gradient experienced by the specimens was shown in Fig. 11.33. There were three to four specimens of each type and they had the same amount of dimensional change within the repeatability of the measurements: $\pm 0.5\%$ of the specimens length (± 0.003 in.). Shielded and nonshielded specimens of a nonboronated graphite were also included in the experiment. The dimensional changes in this material were less than in the boronated graphite, and there was no shielding or size effect.

Irradiation-induced dimensional change data for all warm-pressed and baked boronated graphite specimens are shown in Fig. 11.36 as a function of fast-neutron exposure. The results of the BG-1 experiment and other available data in the literature (Refs. 18, 19) are included. Bands are drawn to enclose most of the data points and indicate the trend.

Except for one data point, the boronated graphite of other workers that was made with a B_4C particle size of less than $20\ \mu m$ showed irradiation-induced dimensional change comparable to the BG-1 boronated graphites containing 100- to $400\text{-}\mu m$ diameter B_4C particles.

The BG-1 specimens containing B_4C made with B^{10} -depleted boron showed a tendency to expand less in either direction than most of the materials

M-28665-1 (a) 3685-111-1000

M-28662-1 (b) 3685-111-2000

0.58
in.

M-28660-1 (c) 3912-34-1000

M-28668-1 (d) 3685-118-1000

0.58
in.

Fig. 11.32--Specimens irradiated in capsule BG-1 to an exposure of 2×10^{21} to 4×10^{21} n/cm² ($E > 0.18$ Mev): (a) warm-pressed boronated (22 wt-% B) graphite; (b) warm-pressed boronated (22 wt-% B) graphite; (c) extruded boronated (28 wt-% B) graphite; and (d) warm-pressed graphite (0 wt-% B). The appearance of these specimens was typical for all materials of similar boron content

M-28659-2

(b)

6X

Fig. 11.33-- B^{10} isotope burnup in boronated graphite (3912-34; 28 wt-% B) during irradiation in capsule BG-1: (a) burnup gradient across specimens; and (b) irradiated specimen with isotopic sample removed from exposed and shielded sides

Table 11.1
SUMMARY OF MATERIAL PROPERTIES AND IRRADIATION EFFECTS IN BORONATED GRAPHITE INCLUDED IN THE CAPSULE BG-1 EXPERIMENT

	Extruded and Baked		Warm-pressed and Baked										
	Peach Bottom HTGR Control Rod Absorber	Unleached Commercial B ₄ C in Graphite (low boron content)	Leached Commercial B ₄ C in Graphite (low boron content)	Leached Commercial B ₄ C in Graphite (high boron content)	Natural Boron Laboratory B ₄ C in Graphite	B ¹⁰ Depleted Boron Laboratory B ₄ C in Graphite	Nonboronated Graphite						
Material No	3912-34	3685-111	3685-111	3685-117	3685-117	3685-124	3685-124	3685-122	3685-122	3685-120	3685-120	3685-118	3685-118
Bake temperature, °C	(a)	1000	2000	1000	2000	1000	2000	1000	2000	1000	2000	1000	2000
No. of specimens													
Unshielded, 0.2 x 0.2 x 0.58 in	0	4	4	0	0	0	0	0	0	0	0	3	0
Shielded, 0.2 x 0.2 x 0.58 in	0	4	4	0	0	0	0	0	0	0	0	3	0
Shielded, 0.2 x 0.4 x 0.58 in	3	4	4	3	3	4	4	3	3	4	4	3	3
Boron content, wt-%	28	22	22	21	21	35	35	23	23	22 ^b	22 ^b	0	0
Boron dispersion, g boron/cm ³	0.45	0.38	0.38	0.36	0.36	0.52	0.52	0.40	0.40	0.38	0.38	0	0
Density, g/cm ³	1.60	1.77	1.79	1.71	1.72	1.68	1.71	1.73	1.72	1.72	1.72	1.77	1.77
Fast-neutron exposure (E>0.18 Mev) 10 ²¹ n/cm ²	2.0-2.5	2.5-3.5	3.5-4.0	2.0-2.5	3.5-4.0	2.5-3.0	3.0-3.5	2.5-3.0	2.0-2.5	3.5-4.0	3.5-4.0	3.5-4.0	3.5-4.0
Thermal-neutron exposure (E<0.4 ev) 10 ²¹ n/cm ²	1.5	1.5	2.5	1.5	2.5	1.5	1.5	1.5	1.5	2.5	2.5	2.5	2.5
Specimen irradiation temp, °C													
Surface minimum	500	525	550	500 ^e	525	550	550 ^e	550	550	575	525	450	525
Surface maximum	575	600	625	575 ^e	575	650	650 ^e	625	625	625	625	575	575
Surface average	550	550	600	550 ^e	550	600	600 ^e	600	600	600	575	475	550
Centerline minimum	600 ^e	625	700	600 ^e	675 ^e	675	675 ^e	675 ^e	675 ^e	675 ^e	625	550 ^e	675 ^e
Centerline maximum	675 ^e	750	825	675 ^e	775 ^e	850	850 ^e	825 ^e	825 ^e	700 ^e	700	600 ^e	775 ^e
Centerline average	650 ^e	675	750	650 ^e	700 ^e	750	750 ^e	750 ^e	750 ^e	675 ^e	650	550 ^e	700 ^e
B ¹⁰ isotope burnup, % depletion													
At neutron window	80	80	67 & 66	80 ^d	75 ^d	66	76	80 ^d	80 ^d	(e)	(e)	0	0
Midspecimen	(f)	30	41	35 ^d	35 ^d	35 ^d	35 ^d	35 ^d	35 ^d	(e)	(e)	0	0
Opposite neutron window	37	53	54 & 43	37 ^d	50 ^d	38	48	50 ^d	50 ^d	(e)	(e)	0	0
Dimensional change during irradiation relative to forming direction, %													
Parallel minimum	-0.52	+2.35	+1.50	+1.53	+2.38	+1.76	+0.31	+2.25	+1.72	+1.05	+1.20	+1.23	+0.12
Parallel maximum	-0.03	+2.81	+3.24	+2.65	+3.22	+2.67	+2.23	+2.75	+2.58	+1.87	+2.15	+1.73	+0.91
Parallel average	-0.23	+2.51	+2.35	+2.01	+2.78	+2.20	+1.26	+2.50	+2.12	+1.42	+1.59	+1.40	+0.44
Perpendicular minimum	-0.13	-0.58	-1.21	-0.75	-0.37	-0.43	-1.09	-0.25	-0.75	-0.68	-1.50	-1.21	-0.96
Perpendicular maximum	+0.86	+0.91	+0.56	+0.89	+0.92	+1.54	+0.91	0	+0.75	+0.31	+0.17	-0.41	-0.10
Perpendicular average	+0.69	+0.24	-0.19	+0.15	+0.37	+0.44	0.00	-0.12	-0.12	-0.16	-0.68	-0.80	-0.63
Graphite matrix crystallite dimension, Å													
Preirradiation L _c	580	490	675	625	850	675	580	675	770	850	675	850	770
Preirradiation C ₀	6.729	6.719	6.715	6.721	6.721	6.715	6.726	6.736	6.710	6.715	6.710	6.715	6.724
Postirradiation L _c	103	200	119	205	117	210	111	(f)	(f)	170	111	195	200
Postirradiation C ₀	6.820	6.736	6.745	6.739	6.782	6.729	6.758	(f)	(f)	6.742	6.768	6.775	6.756
Coefficient of thermal expansion relative to forming direction from 22° to 700°C, 10 ⁻⁶ in./in.-°C													
Preirradiation parallel	3.68	5.57	6.43	7.11	6.60	6.32	5.30	7.05	6.64	6.80	6.18	9.37	9.06
Preirradiation perpendicular	4.70	4.29	3.72	3.40	3.87	4.70	3.82	4.05	4.27	4.26	4.12	4.19	4.01
Ratio (σ _{pa} /σ _{pe})	1.3 ^f	1.3	1.7	2.3	1.7	1.3	1.4	1.7	1.6	1.6	1.5	2.2	2.2
Postirradiation parallel	(f)	8.17	10.15	8.23	8.44	(f)	(f)	(f)	(f)	(f)	(f)	10.41	10.45
Postirradiation perpendicular	(f)	5.25	6.16	5.08	4.34	(f)	(f)	(f)	(f)	(f)	(f)	5.51	5.51
Ratio (σ _{pa} /σ _{pe})	(f)	1.6	1.6	1.6	1.9	(f)	(f)	(f)	(f)	(f)	(f)	(f)	1.9
Thermal conductivity perpendicular to forming direction, cal/cm-sec-°C													
Preirradiation 25°C	0.120	(f)	(f)	0.135	0.120	(f)	(f)	0.100	(f)	(f)	(f)	0.170	(f)
Preirradiation 400°C	0.090	(f)	(f)	0.105	0.095	(f)	(f)	0.075	(f)	(f)	(f)	0.110	(f)
Preirradiation 800°C	0.065	(f)	(f)	0.080	0.070	(f)	(f)	0.060	(f)	(f)	(f)	0.090	(f)
Postirradiation 25°C	(f)	(f)	(f)	0.031	0.015	(f)	(f)	(f)	(f)	(f)	(f)	0.027	(f)
Postirradiation 400°C	(f)	(f)	(f)	0.032	0.018	(f)	(f)	(f)	(f)	(f)	(f)	0.033	(f)
Postirradiation 800°C	(f)	(f)	(f)	0.030	0.020	(f)	(f)	(f)	(f)	(f)	(f)	0.035	(f)
Room temperature flexural strength, psi													
Preirradiation parallel ^h	3300	685	230	595	490	470	310	730	690	(f)	(f)	660	475
Preirradiation perpendicular ^h	1840	995	450	825	930	700	630	1110	1140	(f)	(f)	1100	795
Postirradiation parallel ^h	(f)	770	335	575	620	(f)	(f)	(f)	(f)	(f)	(f)	450	600

^aOriginal bake temperature unreported (<2450°C), specimens baked 2 hr at 1000°C in the laboratory after shaping

^bBoron depleted in B¹⁰ (1.45 at-% B¹⁰, remainder B¹¹)

^cEstimated temperatures based on indicated temperatures of adjacent specimens

^dEstimated B¹⁰ burnup based on measured burnup of specimens nearby

^eBurnup of B¹⁰ not detectable because of initial low B¹⁰ content and probable cross-contamination of specimens

^fNot measured

^gRatio σ_{pa}/σ_{pe} (extruded)

^hLong axis of test bar parallel or perpendicular to the forming direction

Fig. 11.34--Effect of fast-neutron exposure on dimensional change in extruded and baked boronated graphite

Fig. 11.35--Effect of specimen size and thermal-neutron shielding on irradiation-induced dimensional change; all specimens were irradiated in the BG-1 experiment

Fig. 11.36--Effect of fast-neutron exposure on dimensional change of warm-pressed boronated graphite; the boron content for each specimen is given in wt-%

containing natural boron. This observation would seem to indicate that B^{10} content might influence dimensional change; however, a set of specimens containing 35 wt-% natural boron with a similar exposure also showed much less expansion in the parallel direction than did other boronated graphites. The dimensional change of the B^{10} -depleted boronated graphite may therefore have been within the range of experimental variation in dimensional change of all boronated materials in this work.

The dimensional change for boronated graphite and nonboronated warm-pressed graphite are compared in Fig. 11.36. With one notable exception, the nonboronated graphite contracted more in the perpendicular direction and expanded less in the parallel direction than did the boronated graphite. The exception in this generalization was a set of specimens in the BG-1 experiment baked at 1000°C. The irradiation-induced expansion of those specimens in the parallel direction was considerably greater than that of nonboronated graphites and was about the same as that of the B^{10} -depleted boronated graphites and the graphite containing 35 wt-% natural boron (see Fig. 11.36). The high expansion of the nonboronated graphite might be ascribed to its low bake temperature (1000°C), but bake temperature did not influence dimensional changes in boronated graphite. It appears that much of the variation and scatter in irradiation-induced dimensional change data in both boronated and nonboronated graphite must result from undetected differences either in material properties or irradiation conditions.

An examination of the results of this experiment and previous work (see Figs. 11.35 and 11.36) suggests several generalizations that can be made. Irradiation-induced dimensional change in boronated graphite was not sensitive to the following:

1. B_2O_3 content of the B_4C raw material in the range 0 to 1 wt-% B_2O_3 .
2. Specimen size or B^{10} burnup gradient.
3. Bake temperature (1000° to 2000°C).
4. B_4C particle size (less than 400 μm).
5. Natural boron content in the range 20 to 35 wt-% boron.

These results were contrary to predictions that might be made on the basis of a dimensional change model for boronated graphite for the relatively small specimens that were irradiated.

Fast-neutron exposure was the parameter that showed the best correlation with irradiation-induced dimensional change. This was the result predicted by a model for large neutron absorbers. The size or shielding effects must therefore be minimal.

The observation that B_4C particle size did not affect dimensional change showed that localized graphite matrix damage from B^{10} fission fragments was not important. The reduction in dimensional change achieved when B_4C was not present in the graphite matrix, however, implied that B_4C in some manner enhanced expansion of boronated graphite.

Boron (both B^{11} and B^{10}) in solution in graphite single crystals can produce accelerated nucleation of irradiation damage and dimensional change (Ref. 20). Small amounts of boron in the graphite matrix, therefore, might make it more sensitive to fast-neutron damage than nonboronated graphite.

Swelling of B_4C granules because of fast-neutron damage is a second possible mechanism for the observed dimensional change (Ref. 21). Normally, only the $B^{10}(n,\alpha)Li^7$ reaction is considered when damage in B_4C is discussed. In view of the results of this work, a determination of the importance of fast-neutron damage in B_4C expansion is warranted.

A third mechanism contributing to dimensional change might be the complex interaction between the graphite matrix (itself subject to considerable variation in properties) and the dispersed B_4C granules. Dimensional change of both components could produce the situation in which the graphite matrix would attempt to shrink down around the unyielding or expanding B_4C granules.

In spite of the uncertainties about the mechanisms leading to dimensional change, the absence of a significant size or shielding effect seems well established. Therefore, use of boronated graphite irradiation data directly, rather than treating it with analytical models, would provide the most reliable prediction of absorber dimensional change during HTGR service.

Capsule BG-2

The purpose of capsule BG-2 is to measure the effects of neutron irradiation on boronated (B or B_4C plus graphite) and hafnated (HfC plus graphite) compacts. Twelve different materials will be included, using a total of 37 specimens. A summary of materials and the expected results is shown in Table 11.2. Irradiation-induced dimensional change will be the phenomenon of primary interest; however, there will also be correlations of thermal expansion coefficient, thermal conductivity, strength, and microstructure with neutron exposure.

As shown in the table, some specimens from capsule BG-1 will continue irradiation in capsule BG-2. The dimensions of these specimens will be about 0.2 by 0.4 by 0.58 in. or 0.2 by 0.2 by 0.58 in. Unirradiated specimens in BG-2 will be right cylinders 0.4 in. in diameter by 0.58 in. long. The capsule design will provide Cd and Gd thermal-neutron shielding of the specimens on their ends and for 270° around their axes. The shielding will simulate the exposure and produce B^{10} and Hf^{186} burnup gradients that would be experienced by a small segment of a large absorber.

Start of irradiation in the ETR has been scheduled for June in cycle 103. Four irradiation cycles are planned. Specimens will receive a fast-neutron exposure of 2 to 4×10^{21} n/cm², which is equivalent to two to four years of 100% duty in an HTGR core.

Specimen fabrication is under way.

Table 11.2
MATERIALS SCHEDULED FOR IRRADIATION IN CAPSULE BG-2

Cell No.	Crucible No.	Specimen No.	Specimen Description	Comments
1	1	1-1-1, 1-1-2	Peach Bottom HTGR absorber material (28 wt-% B), irradiated in capsule BG-1 (material 3912-34)	Continuation of irradiation from BG-1 to a total exposure of about 4×10^{21} n/cm ² ($E > 0.18$ Mev) to obtain further irradiation data on current production material; correlation of dimensional change in capsule specimens and full-sized HTGR absorbers may eventually be possible.
1	2	1-2-1 (3 specimens)		
1	2	4-1-2,	Warm-pressed boronated graphite (35 wt-% B), baked at 2000°C, irradiated in capsule BG-1 (material 3685-124-2000).	Continuation of irradiation from BG-1 to a total exposure to 5×10^{21} n/cm ² ($E > 0.18$ Mev) to obtain irradiation data on high boron material at significant portion of HTGR life for control rods; study the effect of bake temperature, boron content, and B ¹⁰ burnup gradient on irradiation effects in warm-pressed and baked compacts.
1	3	4-2-1, 4-2-2 (3 specimens)		
1	4	4-3-1, 4-3-2 (2 specimens)	Warm-pressed boronated graphite (35 wt-% B), baked at 1000°C, irradiated in capsule BG-1 (material 3685-124-1000).	
1	5	3-6-1, 3-7-1 (2 specimens)	Warm-pressed graphite (0 wt-% B), baked at 1000°C, irradiated in capsule BG-1 (material 3685-118-1000).	
1	6	1-4-2, 1-4-3, 1-5-2, 1-5-3 (4 specimens)	Warm-pressed boronated graphite (22 wt-% B), baked at 1000°C, irradiated in BG-1 (material 3685-111-1000).	
1	7	3-2-1, 3-2-2 (2 specimens)	Warm-pressed boronated graphite (22 wt-% B), baked at 1000°C, made with B ₄ C depleted in B ¹⁰ , irradiated in capsule BG-1.	
2	1,4,7	2-1, 2-4, 2-7 (3 specimens)	Warm-pressed hafnated graphite (90 wt-% Hf), baked at 2600°C; particle size <325 mesh.	Determine dimensional stability of hafnated graphite as a function of bake temperature.
2	5,6	2-5, 2-6 (2 specimens)	Warm-pressed hafnated graphite (90 wt-% Hf), baked at 2000°C; particle size <325 mesh.	
2	2,3	2-2, 2-3 (2 specimens)	Warm-pressed graphite (0 wt-% B, Hf), baked at 2000°C.	Determine irradiation effects in matrix material used for warm-pressed hafnated and boronated graphites baked at 2000°C.
3	1,2	3-1, 3-2 (2 specimens)	Peach Bottom HTGR absorber material (28 wt-% B), cut from same absorber as specimens in BG-1 (material 3912-34).	New sample of same material as irradiated in BG-1; results from BG-2 irradiation will be correlated with BG-1 results.
3	3,4,5,6,7	3-3, 3-4, 3-5 3-6, 3-7	Warm-pressed absorber from production run (~38 wt-% B).	Determine irradiation effects in current production material; correlation of dimensional change in capsule samples and full-sized HTGR absorbers may eventually be possible.
4	1	4-1 (6 specimens)		
4	2,3,4	4-2, 4-3, 4-4 (3 specimens)	Warm-pressed graphite with 7 wt-% B (91% B ¹⁰ -enriched) added as an amorphous powder, baked at 2000°C.	Determine irradiation effects using amorphous B ¹⁰ -enriched boron powder in graphite to achieve a refractory, low-density absorber material with nuclear worth equivalent to boronated graphite containing ~40 wt-% B.
4	5,6,7	4-5, 4-6, 4-7 (3 specimens)	Warm-pressed boronated graphite with 10 wt-% B ₄ C made from 91% B ¹⁰ -enriched boron, baked at 2000°C.	Determine if use of B ¹⁰ -enriched B ₄ C results in less dimensional change than was observed in compacts of equivalent worth made with amorphous B ¹⁰ powder.

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Appendix A

DESIGN CRITERIA DC-22-2

STEAM-TO-STEAM REHEATER

Document Transmittal To

L. Cavallaro	D. G. Roberts
N. M. Dave'	L. L. Swanson
D. E. Davis	G. L. Wessman
R. H. Graham	J. Yampolsky
J. M. Krase	

From	R. W. Palmer <i>R.W.P.</i>	Date	2/26/69	Proj. No.	317
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Subject Steam-to-Steam Reheater

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Description	Issue	Date
DC-22-2 Steam-to-Steam Reheater	1	2/26/69
<div style="display: flex; justify-content: space-between;"> <div> <p>APPROVED</p> <p><i>L. Cavallaro</i></p> <p><i>G. L. Wessman</i></p> <p><i>J. Yampolsky</i></p> </div> <div> <p>DATE</p> <p><i>Mar. 28 1969</i></p> <p><i>April 1, 1969</i></p> <p><i>4/1/69</i></p> </div> </div>		

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STEAM-TO-STEAM REHEATER

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STEAM TO STEAM REHEATER

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1.0 INTRODUCTION

1.1 SCOPE:

This design criterion covers the design requirements of steam-to-steam reheaters for the High Temperature Gas Cooled Reactor power plant (HTGR) Base Program.

1.2 FUNCTIONAL DESCRIPTION:

The steam-to-steam reheater forms part of the secondary coolant system. It will be located in the crossover between the IP and LP turbines. It will serve to raise the steam temperature in order to avoid excessive moisture developing in the final stages of the LP turbine.

Main steam at 2400 psig 1000°F provides the heat source to the exchanger. This steam passes through the tubes of a counter-current, shell and tube heat exchanger and heat is given up to the cross-over steam passing on the shell side. In the heat transfer process, the main steam is condensed and sub-cooled. The condensate is then pumped back into the boiler feed system. (Refer to Figure 1).

2.0 REFERENCE DOCUMENTS

- a) Standards of the Tubular Exchanger Manufacturer's Association (T.E.M.A.)
- b) Section VIII of ASME Code
- c) Applicable ASME Code Cases
- d) DC-1 General Design Criteria
- e) DC-3 Plant Arrangement

3.0 DESIGN LIFE AND OBJECTIVES

3.1 OBJECTIVE:

This design effort must result in an economical and reliable heat exchanger which is capable of meeting the conditions and requirements stated herein.

3.2 DESIGN LIFE:

The steam-to-steam reheaters shall be designed for an operating life of 30 years at a design capacity factor of 80% (210,000 hours of equivalent full power operation).

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4.0 PERFORMANCE AND OPERATING CONDITIONS

4.1 FULL LOAD:

Full load operation will form the bases for design. Heat transfer conditions are shown in Figure 2 and the overall steam cycle conditions are given in Figure 1.

4.2 TRANSIENTS AND PART LOAD OPERATION:

The design shall consider the effect of transient and part-load operation upon the steam-to-steam reheater expansion and thermal gradients. Details of transient operation are to be determined. Details of transient operation will be determined by GGA if detail design work is done.

4.3 ALLOWABLE PRESSURE LOSSES:

Reheat steam pressure losses including allowances for inlet, outlet, and flow distribution shall not exceed 7.0 psi.

Losses in the high pressure extraction steam circuit, including inlet and outlet losses, shall not exceed 200 psi.

5.0 MECHANICAL DESIGN

5.1 APPLICABLE CODES AND STANDARDS:

Design of the steam-to-steam reheater shall meet the requirements of the ASME Code Section VIII and TEMA Standards for Class R heat exchangers.

5.2 NON-CODE PARTS:

All non-code parts should be designed to the stress limits specified in Section VIII of the ASME Code. All materials must be identifiable and fabrication techniques shall conform to the TEMA Standards when applicable and/or with approved shop practice.

5.3 SEISMIC AND HANDLING SHOCK LOADS:

The steam-to-steam reheater shall be designed for the following specified seismic and handling load criteria.

Earthquake loads	0.25g
Handling shock load	3.0g (any direction)

The reheater shall be capable of withstanding the anticipated earthquake loads superimposed upon the combined design loads caused by piping structural forces, thermal expansion, and pressure. The total stress, excluding stress concentration factors shall not exceed the yield strength of the material.

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<p>5.4 <u>SUPPORTS AND LIFTING DEVICES:</u></p> <p>Supports and lifting devices shall be provided capable of safely carrying the entire weight of the reheater during handling and operation. Supports shall provide for thermal expansion of the shell. Lifting attachments shall withstand the anticipated handling loads without bending or distorting the units.</p> <p>5.5 <u>VENTS AND DRAINS:</u></p> <p>The design shall assure complete venting and draining of the heat exchanger by means of gravity. Both the shell and tube sides shall be provided with one inch socket weld vent and drain pipe connections.</p> <p>5.6 <u>TUBE PLUGGING:</u></p> <p>The design must allow for rapid entry and sufficient space for the manual plugging of tubes.</p> <p>6.0 <u>MATERIALS</u></p> <p>All high alloy material to be used as part of the steam-to-steam reheater shall be stress/corrosion resistant. Where possible, the materials used shall be chosen from those listed in Section 8 of TEMA Standards.</p> <p>7.0 <u>REMOVABILITY</u></p> <p>The design must consider that the reheater tube bundles may be removed from time to time for inspection and maintenance purposes. The support system must allow that the tube bundles can be safely pulled from the vessel; also that pulling eyes will be provided.</p>			
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STEAM TO STEAM REHEAT CYCLE BP7
(2538 MWt)

PRELIMINARY

STEAM TO STEAM REHEATER DESIGN CONDITIONS

Figure 2

Appendix B

DESIGN CRITERIA DC-21-3

SUPERHEAT STEAM TURBINE DRIVEN CIRCULATOR

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Subject SUPERHEAT STEAM TURBINE DRIVEN HELIUM CIRCULATOR								
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<table border="1" style="width:100%; border-collapse: collapse;"> <thead> <tr> <th style="width:80%;">Description</th> <th style="width:10%;">Issue</th> <th style="width:10%;">Date</th> </tr> </thead> <tbody> <tr> <td style="padding: 10px; vertical-align: top;"> DC-21-3 Design Criteria for the superheat steam turbine driven helium circulators applicable to the 1000 Mw(e) Base Program, Task I Base Case This criteria lists requirements for steam turbine drives both preceding and following the superheater. </td> <td style="text-align: center; vertical-align: top;">B</td> <td style="text-align: center; vertical-align: top;">2/20/69</td> </tr> </tbody> </table>	Description	Issue	Date	DC-21-3 Design Criteria for the superheat steam turbine driven helium circulators applicable to the 1000 Mw(e) Base Program, Task I Base Case This criteria lists requirements for steam turbine drives both preceding and following the superheater.	B	2/20/69		
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SUPERHEAT STEAM TURBINE DRIVEN HELIUM CIRCULATOR

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DESIGN CRITERIA

DC-21-3

SUPERHEAT STEAM TURBINE DRIVEN HELIUM CIRCULATOR

BASE PROGRAM, TASK I

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SUPERHEAT STEAM TURBINE DRIVEN HELIUM CIRCULATOR			
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<p>1. <u>SCOPE</u></p> <p>This document contains design criteria for series, superheat steam-driven, helium circulators to be used in a 1000 Mw(e) nuclear power plant. Criteria for the helium shutoff valve, the circulator auxiliary system and circulating handling equipment are not included in this document.</p> <p>2. <u>MAIN FUNCTIONAL REQUIREMENTS</u></p> <p>2.1 The main function of the circulators is to provide helium flow to the primary cooling system.</p> <p>2.2 The power required to drive each helium circulator will be supplied directly from the main steam flow before it enters the high pressure element of the main steam turbine and (1) either after it leaves the superheater or (2) before it enters the superheater. The steam turbines driving the circulators are in series with the main power generating turbine.</p> <p>2.3 The main function of the circulator penetration piping is to provide supply and exhaust flow passages for the circulator steam turbine drive.</p> <p>2.4 Another function of the penetration piping assembly is to provide a secondary hold down device which will prevent the expulsion of the circulator and the related parts out of the PCRV cavity in the case of failure of the primary closure.</p> <p>2.5 The penetration piping assembly will act as a secondary closure designed to contain reactor pressure in case of primary closure failure.</p> <p>3. <u>GENERAL CONFIGURATION</u></p> <p>3.1 The helium circulator will include a single stage compressor and a single stage turbine.</p> <p>3.2 The helium compressor and the steam turbine will be of the fixed geometry type, mounted on a vertical single rotor.</p> <p>3.3 Circulator gravity orientation is such that the compressor wheel is below the turbine wheel.</p> <p>3.4 The circulator is mounted within a reactor access penetration which has a diameter of 12'9" (determined by the steam generator requirements).</p> <p>4. <u>AERODYNAMIC PERFORMANCE CRITERIA</u></p> <p>4.1 The circulator must be capable of operating without flow instability or surge at the 100% operating condition shown in Table 1, and also at various part load conditions to be determined. Note: There are two (2) options being</p>			
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considered for the turbine drive conditions, hence there are two sets of steam conditions listed in Table I.

TABLE I

100% Load Operating Conditions

a. Helium

Flow rate (lb/sec)	410
Inlet pressure (psia)	537
Inlet temperature (°F)	612
Static pressure rise (psia)	13.0

b. Steam

	<u>Case 1</u> <u>After Superheater</u>	<u>Case 2</u> <u>Before Superheater</u>
Flow rate (lb/sec)	277.5	277.
Inlet pressure (psia)	3225.	4205.
Inlet temperature (°F)	1005.	850.
Outlet pressure (psia)	2515.	2880.

4.2 Point of compressor surge, defined as an abrupt drop in pressure rise at low flows for a constant speed, should occur at less than 70% of the full load flow at the design speed.

4.3 Nominal circulator overall efficiency is 64% . Overall efficiency is defined as the product of the compressor and the drive turbine isentropic efficiencies based on conditions at the inlet entrance and diffuser discharge for each machine excluding bearing losses.

4.4 Individual circulator shaft friction must not exceed 200 hp at full plant load.

5. BEARINGS AND SEALS

5.1 All circulator bearings will be water lubricated.

5.2 No mechanical contact is allowed between rotating and stationary parts at any time during the circulator operation.

5.3 Within the full operating speed range, the radial bearings should withstand dynamic loads due to the unbalance of a minimum 40-inch ounce in the compressor wheel plane and a minimum of 12-inch ounce in the turbine wheel plane. It can be assumed that both unbalance loads do not occur simultaneously.

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5.4 In addition to the normal operating loads, the thrust bearing and seal systems must withstand loads due to the following accident conditions:

- a. Turbine exhaust pipeburst causing overspeed and shutdown to zero speed.
- b. Sudden reactor cavity depressurization combined with full steam pressure.

5.5 Land clearance in the thrust bearing should be larger than 0.002 inches, at any normal operating or accident condition.

5.6 In addition to the limited leakage shaft seals for which functional requirements are described in 6.2 and 6.3, a stationary face type shaft seal (called the "shutdown seal") is required at the helium compressor shaft end. The functional shutdown seal is to prevent leakage from or to reactor cavity when the circulator is shut down.

Shutdown seal actuation must be prevented by instrumentation interlock during shaft rotation.

5.7 Each circulator is required to have a mechanical brake or an equivalent device, which must be capable of stopping the rotor rotation from 5% of the full load speed down to zero speed.

Rotor brake actuation must be prevented by instrumentation interlock above 5% speed.

6. DRAINAGE & LEAKAGE

6.1 Because of the circulator orientation, all drains which may flow from the bearings and/or seals must overcome a minimum gravity head of 13.5 feet.

6.2 Essentially no leakage at any time is allowed from the reactor cavity into the circulator bearings or secondary containment area. If required, a limited amount of purified helium is allowed to leak from the circulator into the reactor cavity. No other type of fluid is allowed to leak from the circulators into the reactor cavity.

6.3 A limited amount of steam is allowed to leak from the steam passages into the circulator. No leakage is allowed from the circulator into the steam channels while the steam turbine is operational.

7. ROTOR AND CONTAINMENT STRUCTURAL REQUIREMENTS

7.1 All the rotor critical speeds should be at least 25% above the excitation frequencies throughout the operating speed range.

7.2 All the blades natural frequencies should be at least 25% above the excitation frequencies throughout the operating speed range.

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7.3 No permanent damage shall result from the circulator exceeding rated speed due to a single accident such as a sudden primary system depressurization or rupture in the turbine exhaust steam line.

7.4 Fragments resulting from compressor or turbine blade shedding must be contained inside their respective housings under any normal operating or accident conditions. The design objective is to prevent disk rupture by allowing the initial failure to occur in the turbine blade footing, thereby removing the driving force.

8. CODES AND DESIGN CONDITIONS

8.1 All primary coolant closures and joints to be designed per Nuclear Pressure Vessel Code Section III, Class A. These conditions to exist on either side of the closure with atmospheric conditions on the opposite side.

Design pressure differential, psi	605
Design temperature, °F	850

8.2 All structures and joints containing steam to be designed per USAS Piping Code B31.1.0. Refer to Table I for case designation.

	Case 1	Case 2
Design pressure differential, psia	3550.	4600.
Design temperature, °F	1025.	870.

9. INSTRUMENTATION

9.1 The following parameters should be continuously monitored:

- a. Two speed signals based on two different signals.
- b. Sufficient number of displacement probes to define at any instance the mode of the rotor motion.
- c. The shaft seal pressure differential signals which would indicate any reversal in seal flows.
- d. Differential pressure across the compressor.
- e. Compressor inlet temperature and pressure.

10. LIFE REQUIREMENTS

10.1 Assuming full load conditions, a minimum of 30 years life is required for all circulator components.

11. MAINTENANCE REQUIREMENTS

11.1 Preferably no maintenance should be required during the entire life of the machine.

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11.2 Time to remove and replace circulator from cooled and depressurized reactor - 85 hours.

11.3 All parts exposed to flowing contaminated helium must be readily removable with remote tools in the hot service facility.

12. AUXILIARY REQUIREMENTS PER CIRCULATOR

12.1 Flow of purified helium buffer gas required. (See drainage requirements under Section 6.)

12.1.1 Flow at operating pressure - to be determined

12.1.2 Depressurized flow - to be determined

12.2 Pressure differential between buffer gas and primary coolant

12.2.1 (Circulator suction) - rated load - to be determined

12.2.2 (Circulator suction) - depressurized - to be determined

12.3 Bearing water flow - 180 gpm

12.4 Bearing water inlet temperature - 110°F

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Appendix C

DESIGN CRITERIA DC-21-2

ELECTRIC MOTOR DRIVEN HELIUM CIRCULATOR

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From	M. K. Nichols <i>MKN</i>	Date	2-14-69	Proj. No.	317
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Description	Issue	Date
DC-21-2 DESIGN CRITERIA This document defines the design requirements of a motor driven helium circulator for a 1000 MW(e) high temperature gas-cooled reactor (HTGR) pertinent to the Base Program, Task I.	B	2-4-69

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DESIGN CRITERIA

DC-21-2

ELECTRIC MOTOR DRIVEN HELIUM CIRCULATOR

BASE PROGRAM, TASK I

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ISSUE SUMMARY

Issue	Date	Prepared By	Approved By	Purpose of Issue/ Sections Changed
A	12-30-68	<i>MKN</i> M. K. Nichols	<i>McAvallaro</i>	Review
B	2-4-69	<i>MKN</i> M. K. Nichols	<i>[Signature]</i>	Approval/Revised

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ELECTRIC MOTOR DRIVEN HELIUM CIRCULATOR

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1. SCOPE

This document defines the design requirements of a motor driven helium circulator for a 1000 Mw(e) HTGR. Also included in the requirements is the provision of associated helium ducting, helium shutoff valve, a primary closure and a secondary closure.

2. FUNCTIONAL DESCRIPTION

The 1000 Mw(e) HTGR nuclear generating plant consists of six identical loops, each containing one steam generator in series with one helium circulator. The circulators, associated ducting, and motor drives provide means of circulating the helium coolant through the reactor core and steam generators. In the event a circulator is shut down, a shutoff valve will prevent backflow of the helium.

3. DESIGN REQUIREMENTS

3.1 PLANT LIFE will be 30 years with a use factor of 1.0.

3.2 INSTALLATION: The circulator shall be installed on the same vertical centerline as the steam generator. Helium flows up through the steam generator, through the circulator compressor, and then exits horizontally into a core inlet duct. Figure 1 illustrates the general space allocation for the circulator, including compressor, motor drive, ducting, and valve.

3.3 ENVIRONMENT within the primary coolant system normally consists of pressurized helium gas. The following maximum long term continuous operating impurity levels have been established:

<u>Impurity</u>	<u>Maximum Continuous Operating Concentration, ppm</u>
Total Oxidants (CO, CO ₂ , H ₂ O)	10
Hydrogen	80
Nitrogen	100

3.4 COMPRESSOR OPERATING CONDITIONS (per circulator):

3.4.1 The circulator compressor shall be capable of continuous, full speed, high efficiency operation, without surge or flow instability, over the following range of conditions: (constant helium inventory)

	<u>105%</u> <u>Flow</u>	<u>100%</u> <u>Flow</u>	<u>75%</u> <u>Flow</u>	<u>50%</u> <u>Flow</u>	<u>25%</u> <u>Flow</u>
Helium Flow Rate, lb/sec:	431	410	308	205	103
Helium Inlet Pressure, psia:	538	537	525	501	475
Helium Inlet Temperature, degrees F:	620	612	569	520	472
Static Pressure Rise, psi:	14.25	13.0	7.57	3.55	0.97

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3.4.2 Compressor output at normal helium inventory and full rotational speed shall be variable from 40 to 431 lb/sec flow by a remotely operated control mechanism.

3.4.3 No. of startup/shutdown cycles = 1000. Startup and shutdown cycles will result in short-term flow rates of less than 100 lb/sec.

3.4.4 Radiation level at circulator: 2.4×10^6 nvt (effective)
2500 R/hr gamma field

3.4.5 Emergency operating conditions: (Per circulator)

Emergency operation conditions, as described in the following are the result of preliminary projections, and may be revised at a later date:

	<u>Normal Helium Inventory</u>	<u>Low Pressure</u>
Helium flow rate, lb/sec	190	16
Helium Inlet Pressure, psia	355	14.0
Helium Inlet Temperature, degrees F	250	250
Static Pressure Rise, psi	0.8	0.3
Circulator Speed, rpm	1770	3550

3.5 MOTOR PERFORMANCE REQUIREMENTS:

3.5.1 The motor shall be designed to drive the circulator compressor for all conditions listed in Section 3.4. A two speed, vertical, squirrel cage induction motor shall be used. A single speed motor with a pony motor mounted on a common shaft will be considered as a possible alternate.

3.5.2 The penetration area in which the motor can be installed will be maintained at 200 degrees F and nominally 5 psi above reactor pressure. The atmosphere can be purified dry helium or a suitable alternative such as air or nitrogen.

3.5.3 Method of cooling the motor, if required, is optional.

3.5.4 It is intended that the motor will be started with the help of outside electric power when plant operation is initiated from a shutdown condition. If an individual circulator is restarted with the turbine-generator in operation, the power will be derived from the unit auxiliary transformer. Reduced voltage starting will be employed.

If the turbine generator as well as outside electric power are lost, the circulators will be operated with the on-site standby electric generators. From present projections it appears that one circulator, running at half speed, would be required at full helium inventory, while three circulators, running at full speed, would be required with the primary system depressurized.

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3.6 VALVE AND DUCTING DESIGN REQUIREMENTS:

3.6.1 Appropriate ducting shall be provided to route the gas from the steam generator discharge through the circulator compressor and then to the core inlet flow location as shown on Figure 1. The required static pressure rise for the compressor is measured from the duct entrance to the duct discharge.

3.6.2 A shutoff valve shall be provided which shall close to prevent helium backflow through the circulator compressor and steam generator when a circulator (or circulators) is shut down or removed for maintenance.

3.6.3 The maximum allowable leakage past each closed shutoff valve shall be 7.6 lb/sec at helium conditions of 550 psia, 625 degrees F and a ΔP across the valve of 10 psi.

3.6.4 Reverse flow shall not be allowed for more than 5 seconds past a closing shutoff valve.

3.6.5 The shutoff valve shall require not more than 30 seconds to move from full closed to full open.

3.6.6 The allowable pressure loss across an open shutoff valve shall not exceed 5 percent of the compressor operating head during normal full inventory operation and shall not exceed 10 percent of the compressor operating head during depressurized operation.

3.6.7 The shutoff valve shall be designed to open against a pressure differential of 10 psi.

3.6.8 Total number of shutoff valve operating cycles = 425 during the 30 year design life of the plant with a minimum of one cycle per year.

3.6.9 The flow control mechanism can incorporate the functions of the shutoff valve, provided it meets the requirements of both.

3.7 ROTOR AND CONTAINMENT STRUCTURAL REQUIREMENTS:

3.7.1 All the rotor critical speeds shall be at least 25 percent above possible excitation frequencies throughout the operating speed range.

3.7.2 All the blade natural frequencies shall be at least 25 percent above the excitation frequencies throughout the operating speed range.

3.7.3 The primary closure shall be designed in accordance with the ASME Boiler and Pressure Vessel Code, Section III, Class A Nuclear Vessels. The design pressure shall be 605 psia, acting on either side of the closure, with atmospheric pressure on the opposite side. Design temperature shall be 750 degrees F. The secondary closure shall be designed in accordance with the ASME

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Boiler and Pressure Vessel Code, Section III, Class B Nuclear Vessels. The design pressure shall be 605 psia acting on the inside of the closure, with atmospheric pressure on the opposite side. Design temperature shall be 600 degrees F.

3.7.4 Essentially no leakage across the primary or secondary closure shall be permitted. However, a limited amount of purified helium is allowed to leak from the circulator into the reactor cavity.

3.8 GENERAL REQUIREMENTS:

3.8.1 The efficiency of the motor/circulator unit at 100 percent helium flow shall not be less than 70 percent. This value is based on desired minimum component efficiencies of 77 percent for the compressor (including ducting), 95 percent for the motor and 96 percent for the bearings and seals.

3.8.2 Type of lubrication used in the motor and circulator is optional. Effects of radiation levels and shielding requirements must be considered in the lubricant selection.

3.8.3 It is desirable to have the complete circulator drive, including auxiliaries, designed as an integral unit fitting entirely within the secondary containment cavity.

3.8.4 No mechanical contact is allowed between rotating and stationary parts at any time during the circulator operation.

3.8.5 In addition to the limited leakage shaft seals for which functional requirements are described in 3.7, a stationary face type shaft seal (called the "shutdown seal") is required at the helium compressor shaft end. The shutdown seal prevents leakage from or to the reactor cavity when the circulator is shut down.

Shutdown seal actuation must be prevented by instrumentation interlock during shaft rotation.

3.8.6 Each circulator is required to have a mechanical brake or an equivalent device which must be capable of stopping the rotor rotation from 5 percent of the full load speed down to zero speed.

Rotor brake actuation must be prevented by instrumentation interlock above 5 percent speed.

3.8.7 Because of the circulator orientation, all drains which may flow from the bearings and/or seals must overcome a minimum gravity head of 13.5 feet.

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<p>4. <u>INSTRUMENTATION</u></p> <p>4.1 MOTOR PARAMETERS:</p> <p>4.1.1 Speed based on two independent signals</p> <p>4.1.2 Coolant Temperature</p> <p>4.1.3 Winding Temperature</p> <p>4.2 ROTOR MOTION: Sufficient number of displacement probes to define at any instance the mode of the rotor motion.</p> <p>4.3 SEAL PRESSURES: The shaft seal pressure differential signals which would indicate any reversal in seal flows.</p> <p>4.4 COMPRESSOR PARAMETERS:</p> <p>4.4.1 Differential pressure across the compressor</p> <p>4.4.2 Compressor inlet temperature and pressure</p> <p>4.4.3 Flow control position</p> <p>4.4.4 Shut-off valve position.</p> <p>5. <u>SERVICES AVAILABLE</u></p> <p>5.1 PURIFIED HELIUM will be available at a pressure 5 psi above reactor pressure.</p> <p>5.2 HIGH PRESSURE WATER can be made available.</p> <p>5.3 60 HERTZ ELECTRIC POWER will be provided at the voltage level required by the circulator drive motors.</p> <p>6. <u>MAINTENANCE REQUIREMENTS</u></p> <p>6.1 REMOVAL: Time to remove and replace circulator from cooled and depressurized reactor - 85 hours.</p> <p>6.2 SERVICE: All parts exposed to flowing contaminated helium must be readily removable with remote tools in the hot service facility.</p>			
By MKN	Approved	Approved	Page 5 of 6

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MAIN HELIUM CIRCULATOR SPACE ENVELOPE

Appendix D

DESIGN CRITERIA DC-21-C-1

ELECTRIC MOTOR DRIVEN MAIN HELIUM CIRCULATOR

Document Transmittal To G. S. Braun G. C. Thurston L. Cavallaro T. V. Tung N. M. Dave' G. L. Wessman D. G. Roberts J. S. Yampolsky L. L. Swanson		
From M.K. Nichols	Date 2-14-69	Proj. No. 317
Subject ELECTRIC MOTOR DRIVEN MAIN HELIUM CIRCULATOR		
THE DOCUMENTS LISTED BELOW ARE RELEASED FOR ☐ REVIEW ☒ APPROVAL ☐ INQUIRY ☐ PURCHASE ☐ CONSTRUCTION ☐ INFORMATION ☐ COST ESTIMATE ☐ RETURN TO VENDOR ☐ OTHER		
Remarks		
Description		Issue
Design Specification (21-C-1) to enable an outside manufacturer to prepare and submit a proposal for an electric motor driven, main helium circulator applicable to the 1000 Mw(e) high temperature gas-cooled reactor (HTGR) Base Program, Task I.		B
		2-14-69
PLEASE HANDLE DOCUMENTS RELEASED FOR REVIEW OR APPROVAL AS FOLLOWS: COMPLETE THE SECTION BELOW AND RETURN ONE COPY OF THIS TRANSMITTAL FORM TOGETHER WITH THE LISTED DOCUMENTS ON OR BEFORE _____. ☐ REVIEWED / APPROVED — NO COMMENTS ☐ REVIEWED / APPROVED — COMMENTS SHOWN ON DOCUMENTS ☐ REVIEWED / APPROVED — COMMENTS ON SEPARATE LETTER		
		_____ Name Date

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Specification For ELECTRIC MOTOR DRIVEN MAIN HELIUM CIRCULATOR

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ISSUE SUMMARY

Issue	Date	Prepared by	Approved by	Purpose of Issue/ Sections Changed
1	1-13-69	<i>M.N.</i> M. Nichols		Review
2	1-24-69	<i>M.N.</i> M. Nichols		Approval/as noted
A	1-29-69	<i>M.N.</i> M. Nichols	L. Cavallaro <i>LC</i>	
B	2-14-69	<i>M.N.</i> M. Nichols	<i>M.N.</i>	

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Specification For ELECTRIC MOTOR DRIVEN MAIN HELIUM CIRCULATOR

Proj. No. 317

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Issue B

Date 2-14-69

1. INFORMATION AND GENERAL REQUIREMENTS

1.1 SCOPE: This design specification is for electric motor driven, main helium circulators to be used in a 1000 Mw(e), high temperature gas-cooled reactor (HTGR), nuclear power plant. This information is to be used by an outside manufacturer to prepare and submit to GGA a proposal for electric motor driven circulators compatible with the requirements outlined. Included in the requirements is the provision for the associated helium ducting, helium shut-off valve, a compressor flow control, a primary closure, and a secondary closure.

1.2 HTGR SYSTEM: In the HTGR system, helium gas is cycled continuously between a nuclear reactor core and steam generator by means of helium circulators. The helium, serving as a coolant and heat transfer medium, absorbs heat in the reactor core and then, in the steam generators, releases this heat to produce steam which, in turn, is converted to electricity by a Turbo-generator system.

1.3 FUNCTIONAL DESCRIPTION: The HTGR plant has six identical helium flow loops, each containing one steam generator in series with one helium circulator. The helium circulators, associated ducting, and motor drives provide means of circulating the helium coolant through the reactor core and steam generators. In the event a circulator loop is shut down, a shut-off valve will prevent backflow of the helium.

1.4 SUMMARY OF WORK INCLUDED: The design proposal shall delineate all labor, material, equipment, research and development, services, and associated costs required for the design, testing, fabrication, quality control, shipment, and installation of six completely functional circulator units. Each unit is to include a helium compressor, electric motor drive, compressor flow control, helium ducting, helium shut-off valve, primary and secondary closures such that the complete unit, when mounted, becomes an integral and functional part of the reactor containment vessel. In addition, the design proposal shall include a list of the specific technical problems that will require research and development prior to prototype evaluation.

1.5 EXCEPTIONS to the requirements of this specification will be considered providing the seller's quotation describes such deviations and the reasons for offering them.

1.6 DOCUMENT REVIEWS: The buyer's review of documents prepared by the seller is to be considered an evaluation of the document's adequacy and suitability for the intended purpose. Such reviews do not relieve the seller of any assigned responsibilities and the obligation to comply with the buyer's requirements.

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Specification For ELECTRIC MOTOR DRIVEN MAIN HELIUM CIRCULATOR

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2. DESIGN REQUIREMENTS

2.1 PLANT LIFE will be 30 years with a use factor of 1.0.

2.2 INSTALLATION: The circulator shall be installed on the same vertical centerline as the steam generator. Helium flows up through the steam generator, through the circulator compressor, and then exits horizontally into a core inlet duct. Figure 1 illustrates the general space allocation for the circulator, including compressor, motor drive, ducting, and valve.

2.3 ENVIRONMENT within the primary coolant system normally consists of pressurized helium gas. The following maximum long term continuous operating impurity levels have been established:

<u>Impurity</u>	<u>Maximum Continuous Operating Concentration, ppm</u>
Total Oxidants (CO, CO ₂ , H ₂ O)	10
Hydrogen	80
Nitrogen	100

2.4 COMPRESSOR OPERATING CONDITIONS (per circulator):

A. The circulator compressor shall be capable of continuous, full speed, high efficiency operation, without surge or flow instability, over the following range of conditions: (constant helium inventory)

	<u>105% Flow</u>	<u>100% Flow</u>	<u>75% Flow</u>	<u>50% Flow</u>	<u>25% Flow</u>
Helium Flow Rate, lb/sec:	431	410	308	205	103
Helium Inlet Pressure, psia:	538	537	525	501	475
Helium Inlet Temperature, degrees F:	620	612	569	520	472
Static Pressure Rise, psi:	14.25	13.0	7.57	3.55	0.97

B. Compressor output at normal helium inventory and full rotational speed shall be variable from 40 to 431 lb/sec flow by a remotely operated control mechanism to be provided by the seller.

C. No. of startup/shutdown cycles = 1000. Startup and shutdown cycles will result in short-term flow rates of less than 100 lb/sec.

D. Radiation level at circulator: 2.4×10^6 nvt (effective)
2500 R/hr gamma field

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E. Emergency operating conditions: (Per circulator)

Emergency operation conditions, as described in the following, are the result of preliminary projections, and may be revised at a later date:

	<u>Normal Helium Inventory</u>	<u>Low Pressure</u>
Helium flow rate, lb/sec	190	16
Helium inlet pressure, psia	355	14.0
Helium inlet temperature, degrees F	250	250
Static pressure rise, psi	0.8	0.3
Circulator speed, rpm	1770	3550

2.5 MOTOR PERFORMANCE REQUIREMENTS:

A. The motor shall be designed to drive the circulator compressor for all conditions listed in Section 2.4. A two-speed, vertical, squirrel cage, induction motor shall be used. A single speed motor with a pony motor mounted on a common shaft will be considered as a possible alternate.

B. The penetration area in which the motor can be installed will be maintained at 200 degrees F and nominally 5 psi above reactor pressure. The atmosphere can be purified dry helium or a suitable alternative such as air or nitrogen.

C. Method of cooling the motor, if required, is optional. Cooling water provisions, if required, will be done by others.

D. It is intended that the motor will be started with the help of out-side electric power when plant operation is initiated from a shutdown condition. If an individual circulator is restarted with the turbine-generator in operation, the power will be derived from the unit auxiliary transformer. Reduced voltage starting will be employed.

If the turbine-generator as well as outside electric power are lost, the circulators will be operated with the on-site standby electric generators. From present projections it appears that one circulator, running at half speed, would be required at full helium inventory, while three circulators, running at full speed, would be required with the primary system depressurized. The standby generators will be provided by others.

E. The motor power requirements for normal, part-load, startup and depressurized operating conditions shall be listed and discussed in the design proposal.

2.6 VALVE AND DUCTING DESIGN REQUIREMENTS:

A. Appropriate ducting shall be provided to route the gas from the steam generator discharge through the circulator compressor and then to the core inlet flow location as shown on Figure 1. The required static pressure rise for the compressor is measured from the duct entrance to the duct discharge.

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B. A shutoff valve shall be provided which shall close to prevent helium backflow through the circulator compressor and steam generator when a circulator (or circulators) is shut down or removed for maintenance.

C. The maximum allowable leakage past each closed shutoff valve shall be 7.6 lb/sec at helium conditions of 550 psia, 625 degrees F and a ΔP across the valve of 10 psi.

D. Reverse flow shall not be allowed for more than 5 seconds past a closing shutoff valve.

E. The shutoff valve shall require not more than 30 seconds to move from full closed to full open.

F. The allowable pressure loss across an open shutoff valve shall not exceed 5 percent of the compressor operating head during normal full inventory operation and shall not exceed 10 percent of the compressor operating head during depressurized operation.

G. The shutoff valve shall be designed to open against a pressure differential of 10 psi.

H. Total number of shutoff valve operating cycles = 425 during the 30-year design life of the plant with a minimum of one cycle per year.

I. The flow control mechanism can incorporate the functions of the shutoff valve, provided it meets the requirements of both.

2.7 ROTOR AND CONTAINMENT STRUCTURAL REQUIREMENTS:

A. All the rotor critical speeds shall be at least 25 percent above possible excitation frequencies throughout the operating speed range.

B. All the blade natural frequencies shall be at least 25 percent above the excitation frequencies throughout the operating speed range.

C. The primary closure shall be designed in accordance with the ASME Boiler and Pressure Vessel Code, Section III, Class A Nuclear Vessels. The design pressure shall be 605 psia, acting on either side of the closure, with atmospheric pressure on the opposite side. Design temperature shall be 750 degrees F. The secondary closure shall be designed in accordance with the ASME Boiler and Pressure Vessel Code, Section III, Class B Nuclear Vessels. The design pressure shall be 605 psia acting on the inside of the closure, with atmospheric pressure on the opposite side. Design temperature shall be 600 degrees F.

D. Essentially no leakage across the primary or secondary closure shall be permitted. However, a limited amount of purified helium is allowed to leak from the circulator into the reactor cavity.

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2.8 GENERAL REQUIREMENTS:

A. The efficiency of the motor/circulator unit at 100 percent helium flow shall not be less than 70 percent. This value is based on desired minimum component efficiencies of 77 percent for the compressor (including ducting), 95 percent for the motor and 96 percent for the bearings and seals.

B. Type of lubrication used in the motor and circulator is optional. Effects of radiation levels and shielding requirements must be considered in the lubricant selection.

C. It is desirable to have the complete circulator drive, including auxiliaries, designed as an integral unit fitting entirely within the secondary containment cavity.

D. No mechanical contact is allowed between rotating and stationary parts at any time during the circulator operation.

E. In addition to the limited leakage shaft seals for which functional requirements are described in 2.7, a stationary face type shaft seal (called the "shutdown seal") is required at the helium compressor shaft end. The shutdown seal prevents leakage from or to the reactor cavity when the circulator is shut down.

Shutdown seal actuation must be prevented by instrumentation interlock during shaft rotation.

F. Each circulator is required to have a mechanical brake or an equivalent device which must be capable of stopping the rotor rotation from 5% of the full load speed down to zero speed.

Rotor brake actuation must be prevented by instrumentation interlock above 5% speed.

G. Because of the circulator orientation, all drains which may flow from the bearings and/or seals must overcome a minimum gravity head of 13.5 feet.

2.9 ENGINEERING ANALYSIS: With the quotation, the seller shall describe all proposed analyses and design work including:

A. Areas that will be subject to analysis.

B. Proposed methods of analysis including formulae and assumptions. Supporting information in the form of test data and prior service experience, if available, shall be furnished to substantiate the proposals.

The design proposal shall include, but is not limited to, a technical analysis of the following problems.

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- 1) Electrical insulation
- 2) Electrical connectors
- 3) Expected motor performance
- 4) Expected compressor performance
- 5) Bearing and seal systems
- 6) Rotor dynamics
- 7) Compressor flow control
- 8) Auxiliary system requirements
- 9) Start-up modes
- 10) Helium shut-off valve

3. INSTRUMENTATION

3.1 MOTOR PARAMETERS:

- A. Speed based on two independent signals.
- B. Coolant Temperature.
- C. Winding Temperature.

3.2 ROTOR MOTION: Sufficient number of displacement probes to define at any instance the mode of the rotor motion.

3.3 SEAL PRESSURES: The shaft seal pressure differential signals which would indicate any reversal in seal flows.

3.4 COMPRESSOR PARAMETERS:

- A. Differential pressure across the compressor.
- B. Compressor inlet temperature and pressure.
- C. Flow control position.
- D. Shut-off valve position.

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<p>4. <u>SERVICES AVAILABLE</u></p> <p> 4.1 PURIFIED HELIUM will be available at a pressure 5 psi above reactor pressure.</p> <p> 4.2 HIGH PRESSURE WATER can be made available.</p> <p> 4.3 60 HERTZ ELECTRIC POWER will be provided at the voltage level required by the circulator drive motors.</p> <p>5. <u>MAINTENANCE REQUIREMENTS</u></p> <p> 5.1 REMOVAL: Time to remove and replace circulator from cooled and depressurized reactor - 85 hours.</p> <p> 5.2 SERVICE: All parts exposed to flowing contaminated helium must be readily removable with remote tools in the hot service facility. The service and removal equipment will be provided by others.</p> <p>6. <u>RESEARCH AND DEVELOPMENT</u></p> <p> 6.1 DEFINITION OF PROBLEMS: The design proposal shall include a listing and a discussion of the specific technical problems that will require research and development prior to prototype evaluation.</p> <p> 6.2 PROGRAM OUTLINE: The design proposal shall include a research and development program outline covering costs, scope, and the time, manpower and equipment requirements associated with such a program.</p> <p>7. <u>TESTS AND INSTALLATION</u></p> <p> 7.1 SIMULATION TESTS: Tests under simulated operating conditions shall be performed to substantiate that the circulator and its components meet the specified performance requirements and to demonstrate the performance and integrity of the circulator for the life of the plant. If comparable tests have been performed previously, these test results may be submitted for consideration.</p> <p> 7.2 TEST PROCEDURES: The seller shall prepare procedures for the testing of the circulator and shall transmit them to the buyer for review. An outline of the test procedures shall be included in the proposal and shall include, but is not limited to: (1) size of test sample; (2) test environment; (3) type of instrumentation to be used; (4) data to be taken; (5) description of the test facility.</p> <p> 7.3 INSTALLATION: The design proposal shall include a preliminary installation procedure. A detailed installation procedure shall be required prior to shipment of the circulator. The seller shall furnish a representative at the site during the installation of the circulator.</p>			
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VENDOR'S DATA (NOTE: IDENTIFY ALL DATA WITH GULF GENERAL ATOMIC PURCHASE ORDER NUMBER, SERVICE AND EQUIPMENT NUMBER.)				QUOTATION COPIES		APPROVAL COPIES	FINAL COPIES
				PRINTS	DIAZO-MASTERS	PRINTS	DIAZO-MASTERS
OUTLINE DIMENSIONS AND FOUNDATION REQUIREMENTS							3
CROSS SECTIONAL DRAWINGS WITH COMPLETE PARTS LIST							3
PIPING DRAWINGS							
ELECTRICAL DRAWINGS							3
INSTRUMENT DRAWINGS INCLUDING MOUNTING DETAILS							3
SHOP DETAIL AND ERECTION DRAWINGS							
COMPLETED GULF GENERAL ATOMIC DATA SHEETS							
MATERIAL SPECIFICATIONS							
MATERIAL CERTIFICATIONS							
REPRESENTATIVE PERFORMANCE DATA							3
CERTIFIED PERFORMANCE DATA FROM ACTUAL TESTS							
VENDOR'S DESIGN REPORTS AND CALCULATIONS							
VESSEL STRESS REPORT							
MANUFACTURERS' DATA REPORTS FOR VESSELS							
MANUFACTURERS' TEST REPORTS FOR SAFETY VALVES							
FABRICATION PROCEDURES							
WELDING PROCEDURES							
NONDESTRUCTIVE EXAMINATION PROCEDURES							
QUALITY CONTROL PROCEDURES							
INSTALLATION, OPERATION AND MAINTENANCE INSTRUCTIONS							
RECOMMENDED SPARE PARTS AND SPECIAL TOOLS							
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MAIN HELIUM CIRCULATOR SPACE ENVELOPE

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Document Transmittal To G. S. Braun G. C. Thurston - L. Cavallaro T. V. Tung N. M. Dave' G. L. Wessman D. G. Roberts J. S. Yampolsky - L. L. Swanson		
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Design Specification (21-C-1) to enable an outside manufacturer to prepare and submit a proposal for an electric motor driven, main helium circulator applicable to the 1000 Mw(e) high temperature gas-cooled reactor (HTGR) Base Program, Task I.	B	2-14-69
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		Name _____ Date _____

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ISSUE SUMMARY

Issue	Date	Prepared by	Approved by	Purpose of Issue/ Sections Changed
1	1-13-69	M. Nichols		Review
2	1-24-69	M. Nichols		Approval/as noted
A	1-29-69	M. Nichols	L. Cavallaro	
B	2-14-69	M. Nichols		

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Proj. No. 317

Spec. No. 21-C-1

Issue B

Date 2-14-69

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A	1-29-69	<i>MN</i> M. Nichols	L. Cavallaro <i>LC</i>	
B	2-14-69	<i>MN</i> M. Nichols	<i>SHD</i> <i>SHK</i>	

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1. INFORMATION AND GENERAL REQUIREMENTS

1.1 SCOPE: This design specification is for electric motor driven, main helium circulators to be used in a 1000 Mw(e), high temperature gas-cooled reactor (HTGR), nuclear power plant. This information is to be used by an outside manufacturer to prepare and submit to GGA a proposal for electric motor driven circulators compatible with the requirements outlined. Included in the requirements is the provision for the associated helium ducting, helium shut-off valve, a compressor flow control, a primary closure, and a secondary closure.

1.2 HTGR SYSTEM: In the HTGR system, helium gas is cycled continuously between a nuclear reactor core and steam generator by means of helium circulators. The helium, serving as a coolant and heat transfer medium, absorbs heat in the reactor core and then, in the steam generators, releases this heat to produce steam which, in turn, is converted to electricity by a Turbo-generator system.

1.3 FUNCTIONAL DESCRIPTION: The HTGR plant has six identical helium flow loops, each containing one steam generator in series with one helium circulator. The helium circulators, associated ducting, and motor drives provide means of circulating the helium coolant through the reactor core and steam generators. In the event a circulator loop is shut down, a shut-off valve will prevent backflow of the helium.

1.4 SUMMARY OF WORK INCLUDED: The design proposal shall delineate all labor, material, equipment, research and development, services, and associated costs required for the design, testing, fabrication, quality control, shipment, and installation of six completely functional circulator units. Each unit is to include a helium compressor, electric motor drive, compressor flow control, helium ducting, helium shut-off valve, primary and secondary closures such that the complete unit, when mounted, becomes an integral and functional part of the reactor containment vessel. In addition, the design proposal shall include a list of the specific technical problems that will require research and development prior to prototype evaluation.

1.5 EXCEPTIONS to the requirements of this specification will be considered providing the seller's quotation describes such deviations and the reasons for offering them.

1.6 DOCUMENT REVIEWS: The buyer's review of documents prepared by the seller is to be considered an evaluation of the document's adequacy and suitability for the intended purpose. Such reviews do not relieve the seller of any assigned responsibilities and the obligation to comply with the buyer's requirements.

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2. DESIGN REQUIREMENTS

2.1 PLANT LIFE will be 30 years with a use factor of 1.0.

2.2 INSTALLATION: The circulator shall be installed on the same vertical centerline as the steam generator. Helium flows up through the steam generator, through the circulator compressor, and then exits horizontally into a core inlet duct. Figure 1 illustrates the general space allocation for the circulator, including compressor, motor drive, ducting, and valve.

2.3 ENVIRONMENT within the primary coolant system normally consists of pressurized helium gas. The following maximum long term continuous operating impurity levels have been established:

<u>Impurity</u>	<u>Maximum Continuous Operating Concentration, ppm</u>
Total Oxidants (CO, CO ₂ , H ₂ O)	10
Hydrogen	80
Nitrogen	100

2.4 COMPRESSOR OPERATING CONDITIONS (per circulator):

A. The circulator compressor shall be capable of continuous, full speed, high efficiency operation, without surge or flow instability, over the following range of conditions: (constant helium inventory)

	<u>105% Flow</u>	<u>100% Flow</u>	<u>75% Flow</u>	<u>50% Flow</u>	<u>25% Flow</u>
Helium Flow Rate, lb/sec:	431	410	308	205	103
Helium Inlet Pressure, psia:	538	537	525	501	475
Helium Inlet Temperature, degrees F:	620	612	569	520	472
Static Pressure Rise, psi:	14.25	13.0	7.57	3.55	0.97

B. Compressor output at normal helium inventory and full rotational speed shall be variable from 40 to 431 lb/sec flow by a remotely operated control mechanism to be provided by the seller.

C. No. of startup/shutdown cycles = 1000. Startup and shutdown cycles will result in short-term flow rates of less than 100 lb/sec.

D. Radiation level at circulator: 2.4×10^6 nvt (effective)
2500 R/hr gamma field

Notations in this column indicate where changes have been made.

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E. Emergency operating conditions: (Per circulator)

Emergency operation conditions, as described in the following, are the result of preliminary projections, and may be revised at a later date:

	<u>Normal Helium Inventory</u>	<u>Low Pressure</u>
Helium flow rate, lb/sec	190	16
Helium inlet pressure, psia	355	14.0
Helium inlet temperature, degrees F	250	250
Static pressure rise, psi	0.8	0.3
Circulator speed, rpm	1770	3550

2.5 MOTOR PERFORMANCE REQUIREMENTS:

A. The motor shall be designed to drive the circulator compressor for all conditions listed in Section 2.4. A two-speed, vertical, squirrel cage, induction motor shall be used. A single speed motor with a pony motor mounted on a common shaft will be considered as a possible alternate.

B. The penetration area in which the motor can be installed will be maintained at 200 degrees F and nominally 5 psi above reactor pressure. The atmosphere can be purified dry helium or a suitable alternative such as air or nitrogen.

C. Method of cooling the motor, if required, is optional. Cooling water provisions, if required, will be done by others.

D. It is intended that the motor will be started with the help of out-side electric power when plant operation is initiated from a shutdown condition. If an individual circulator is restarted with the turbine-generator in operation, the power will be derived from the unit auxiliary transformer. Reduced voltage starting will be employed.

If the turbine-generator as well as outside electric power are lost, the circulators will be operated with the on-site standby electric generators. From present projections it appears that one circulator, running at half speed, would be required at full helium inventory, while three circulators, running at full speed, would be required with the primary system depressurized. The standby generators will be provided by others.

E. The motor power requirements for normal, part-load, startup and depressurized operating conditions shall be listed and discussed in the design proposal.

2.6 VALVE AND DUCTING DESIGN REQUIREMENTS:

A. Appropriate ducting shall be provided to route the gas from the steam generator discharge through the circulator compressor and then to the core inlet flow location as shown on Figure 1. The required static pressure rise for the compressor is measured from the duct entrance to the duct discharge.

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B. A shutoff valve shall be provided which shall close to prevent helium backflow through the circulator compressor and steam generator when a circulator (or circulators) is shut down or removed for maintenance.

C. The maximum allowable leakage past each closed shutoff valve shall be 7.6 lb/sec at helium conditions of 550 psia, 625 degrees F and a ΔP across the valve of 10 psi.

D. Reverse flow shall not be allowed for more than 5 seconds past a closing shutoff valve.

E. The shutoff valve shall require not more than 30 seconds to move from full closed to full open.

F. The allowable pressure loss across an open shutoff valve shall not exceed 5 percent of the compressor operating head during normal full inventory operation and shall not exceed 10 percent of the compressor operating head during depressurized operation.

G. The shutoff valve shall be designed to open against a pressure differential of 10 psi.

H. Total number of shutoff valve operating cycles = 425 during the 30-year design life of the plant with a minimum of one cycle per year.

I. The flow control mechanism can incorporate the functions of the shutoff valve, provided it meets the requirements of both.

2.7 ROTOR AND CONTAINMENT STRUCTURAL REQUIREMENTS:

A. All the rotor critical speeds shall be at least 25 percent above possible excitation frequencies throughout the operating speed range.

B. All the blade natural frequencies shall be at least 25 percent above the excitation frequencies throughout the operating speed range.

C. The primary closure shall be designed in accordance with the ASME Boiler and Pressure Vessel Code, Section III, Class A Nuclear Vessels. The design pressure shall be 605 psia, acting on either side of the closure, with atmospheric pressure on the opposite side. Design temperature shall be 750 degrees F. The secondary closure shall be designed in accordance with the ASME Boiler and Pressure Vessel Code, Section III, Class B Nuclear Vessels. The design pressure shall be 605 psia acting on the inside of the closure, with atmospheric pressure on the opposite side. Design temperature shall be 600 degrees F.

D. Essentially no leakage across the primary or secondary closure shall be permitted. However, a limited amount of purified helium is allowed to leak from the circulator into the reactor cavity.

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2.8 GENERAL REQUIREMENTS:

A. The efficiency of the motor/circulator unit at 100 percent helium flow shall not be less than 70 percent. This value is based on desired minimum component efficiencies of 77 percent for the compressor (including ducting), 95 percent for the motor and 96 percent for the bearings and seals.

B. Type of lubrication used in the motor and circulator is optional. Effects of radiation levels and shielding requirements must be considered in the lubricant selection.

C. It is desirable to have the complete circulator drive, including auxiliaries, designed as an integral unit fitting entirely within the secondary containment cavity.

D. No mechanical contact is allowed between rotating and stationary parts at any time during the circulator operation.

E. In addition to the limited leakage shaft seals for which functional requirements are described in 2.7, a stationary face type shaft seal (called the "shutdown seal") is required at the helium compressor shaft end. The shutdown seal prevents leakage from or to the reactor cavity when the circulator is shut down.

Shutdown seal actuation must be prevented by instrumentation interlock during shaft rotation.

F. Each circulator is required to have a mechanical brake or an equivalent device which must be capable of stopping the rotor rotation from 5% of the full load speed down to zero speed.

Rotor brake actuation must be prevented by instrumentation interlock above 5% speed.

G. Because of the circulator orientation, all drains which may flow from the bearings and/or seals must overcome a minimum gravity head of 13.5 feet.

2.9 ENGINEERING ANALYSIS: With the quotation, the seller shall describe all proposed analyses and design work including:

A. Areas that will be subject to analysis.

B. Proposed methods of analysis including formulae and assumptions. Supporting information in the form of test data and prior service experience, if available, shall be furnished to substantiate the proposals.

The design proposal shall include, but is not limited to, a technical analysis of the following problems.

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- 1) Electrical insulation
- 2) Electrical connectors
- 3) Expected motor performance
- 4) Expected compressor performance
- 5) Bearing and seal systems
- 6) Rotor dynamics
- 7) Compressor flow control
- 8) Auxiliary system requirements
- 9) Start-up modes
- 10) Helium shut-off valve

3. INSTRUMENTATION

3.1 MOTOR PARAMETERS:

- A. Speed based on two independent signals.
- B. Coolant Temperature.
- C. Winding Temperature.

3.2 ROTOR MOTION: Sufficient number of displacement probes to define at any instance the mode of the rotor motion.

3.3 SEAL PRESSURES: The shaft seal pressure differential signals which would indicate any reversal in seal flows.

3.4 COMPRESSOR PARAMETERS:

- A. Differential pressure across the compressor.
- B. Compressor inlet temperature and pressure.
- C. Flow control position.
- D. Shut-off valve position.

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4. SERVICES AVAILABLE

4.1 PURIFIED HELIUM will be available at a pressure 5 psi above reactor pressure.

4.2 HIGH PRESSURE WATER can be made available.

4.3 60 HERTZ ELECTRIC POWER will be provided at the voltage level required by the circulator drive motors.

5. MAINTENANCE REQUIREMENTS

5.1 REMOVAL: Time to remove and replace circulator from cooled and depressurized reactor - 85 hours.

5.2 SERVICE: All parts exposed to flowing contaminated helium must be readily removable with remote tools in the hot service facility. The service and removal equipment will be provided by others.

6. RESEARCH AND DEVELOPMENT

6.1 DEFINITION OF PROBLEMS: The design proposal shall include a listing and a discussion of the specific technical problems that will require research and development prior to prototype evaluation.

6.2 PROGRAM OUTLINE: The design proposal shall include a research and development program outline covering costs, scope, and the time, manpower and equipment requirements associated with such a program.

7. TESTS AND INSTALLATION

7.1 SIMULATION TESTS: Tests under simulated operating conditions shall be performed to substantiate that the circulator and its components meet the specified performance requirements and to demonstrate the performance and integrity of the circulator for the life of the plant. If comparable tests have been performed previously, these test results may be submitted for consideration.

7.2 TEST PROCEDURES: The seller shall prepare procedures for the testing of the circulator and shall transmit them to the buyer for review. An outline of the test procedures shall be included in the proposal and shall include, but is not limited to: (1) size of test sample; (2) test environment; (3) type of instrumentation to be used; (4) data to be taken; (5) description of the test facility.

7.3 INSTALLATION: The design proposal shall include a preliminary installation procedure. A detailed installation procedure shall be required prior to shipment of the circulator. The seller shall furnish a representative at the site during the installation of the circulator.

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VENDOR'S DATA (NOTE: IDENTIFY ALL DATA WITH GULF GENERAL ATOMIC PURCHASE ORDER NUMBER, SERVICE AND EQUIPMENT NUMBER.)				QUOTATION COPIES		APPROVAL COPIES		FINAL COPIES	
				PRINTS	DIAZO-MASTERS	PRINTS	DIAZO-MASTERS	PRINTS	DIAZO-MASTERS
OUTLINE DIMENSIONS AND FOUNDATION REQUIREMENTS								3	
CROSS SECTIONAL DRAWINGS WITH COMPLETE PARTS LIST								3	
PIPING DRAWINGS									
ELECTRICAL DRAWINGS								3	
INSTRUMENT DRAWINGS INCLUDING MOUNTING DETAILS								3	
SHOP DETAIL AND ERECTION DRAWINGS									
COMPLETED GULF GENERAL ATOMIC DATA SHEETS									
MATERIAL SPECIFICATIONS									
MATERIAL CERTIFICATIONS									
REPRESENTATIVE PERFORMANCE DATA								3	
CERTIFIED PERFORMANCE DATA FROM ACTUAL TESTS									
VENDOR'S DESIGN REPORTS AND CALCULATIONS									
VESSEL STRESS REPORT									
MANUFACTURERS' DATA REPORTS FOR VESSELS									
MANUFACTURERS' TEST REPORTS FOR SAFETY VALVES									
FABRICATION PROCEDURES									
WELDING PROCEDURES									
NONDESTRUCTIVE EXAMINATION PROCEDURES									
QUALITY CONTROL PROCEDURES									
INSTALLATION, OPERATION AND MAINTENANCE INSTRUCTIONS									
RECOMMENDED SPARE PARTS AND SPECIAL TOOLS									
TEST PROCEDURES								3	
RESEARCH AND DEVELOPMENT (PROPOSED PROGRAM)								3	
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MAIN HELIUM CIRCULATOR SPACE ENVELOPE

290

1000 MW (e) HTGR

FIGURE 1

Appendix E

REFERENCE DESIGN PARAMETERS

PUBLIC SERVICE COMPANY OF COLORADO

HTGR POWER PLANT

Appendix E

REFERENCE DESIGN PARAMETERSPUBLIC SERVICE COMPANY OF COLORADO HTGR POWER PLANT

	Design Basis Maximum Guaranteed Point ^a	Turbine Valves Wide Open Max. Expected Point ^b
Over-all Plant Output:		
Main generator output, kw	342,001	356,773
Net station output to transformer, kw	330,201	344,817
Net station heat rate, Btu/Kw-hr	8,700	8,693
Net station efficiency, %	39.23	39.26
Primary Coolant System:		
Reactor thermal power, Mw gross	841,700	878,300
Helium temp., circulator inlet, °F	742	752
Total helium flow rate, lb/hr	3,490,000	3,666,700
Primary system pressure drop, psi	14.0	15.36
Primary system efficiency, %	99.15	99.15
Operating pressure, psia	700	707
Number of loops	2	2
Helium temp., steam gen. inlet, °F	1,427	1,433
Secondary Coolant System:		
Turbine Generator:		
Turbine throttle pressure, psia	2,412	2,412
Turbine throttle temperature, °F	1,000	998
Turbine throttle flow, lb/hr	2,305,326	2,420,500
HP turbine exhaust pressure, psia	891	937
IP turbine inlet pressure, psia	567	596
IP turbine inlet temperature, °F	1,000	1,000
Reheat steam flow, lb/hr	2,249,221	2,366,000
LP turbine backpressure, in. Hg Abs	2.50	2.50
Regenerative Feedwater Cycle:		
Feedwater flow, lb/hr	2,305,326	2,420,500
Final feedwater temperature, °F	403	408
Number of feedwater heaters	6	6
Circulator-Turbine:		
Inlet pressure, psia	870	915
Inlet temperature, °F	738	748
Outlet pressure, psia	649	681
No. of circulators	4	4
Total circulator - turbine power, kw	16,590	19,013
Approx. power margin via bypass flow, % ..	7.0	0.0

Appendix E continued

	Design Basis Maximum Guaranteed Point ^a	Turbine Valves Wide Open Max. Expected Point ^b
Steam Generator:		
Superheater outlet pressure, psia	2,512	2,523
Superheater outlet temperature, °F	1,005	1,005
Reheater outlet pressure, psia	600	630
Reheater outlet temperature, °F	1,002	1,002
Reheater inlet enthalpy, Btu/lb	1,332.8	1,335.4
Reheater pressure drop, psi	36	38
Economizer inlet enthalpy, Btu/lb	382.4	386.8
Reheater steam flow, lb/hr	2,245,366	2,362,200

^aBased upon General Electric Heat Balance 368 HB 481.

^bEstimated by Gulf General Atomic - Heat Balance 194A.