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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

General and specific ground rules for plant economic studies were prepared and are being reviewed. The ground rules will be used in the HTGR Base Program Task I economic assessment studies, and the constant economic parameters will be used as bases for determining the power-generation costs of the design and cycle alternatives that will be considered.

All economic comparisons will be based on establishing the power-generation costs of a plant capable of an 80% operating factor with 90% plant availability and a 30-year lifetime. The economic assessments will consider the plant and operating costs up to the low-voltage side of the stepup transformer and will not consider distribution charges on switchyard costs. The system of accounts outlined in TID-7025, Guide to Nuclear Power Cost Evaluation, will be used (Ref. 1)*.

The cost components will be estimated for the Base Case design at 1000-Mw(e) output and 39.4% plant efficiency. Deviations from these parameters will be accounted for in each alternative under study by the use of capability and plant efficiency adjustment factors. Indirect costs will be assumed to vary with direct costs, even though this may slightly increase the cost differential between cases. Operating and maintenance costs will be considered essentially unchanged, regardless of plant configuration. Fuel cycle costs will assume fuel fabrication and reprocessing facilities capable of 15,000 Mw(e).

The total power generation costs will be determined for each alternative being considered. Although it is recognized that this is not essential to an economic assessment, the use of total power generation costs as a means of comparison will add a measure of perspective in evaluating the design alternatives. Investor-owned utilities will be used as the basis of power generation costs to establish a fixed capital charge of 13.44% for depreciating capital and 12.03% for nondepreciating capital. Nuclear liability insurance and operating and maintenance costs will be assumed to be constant. Exceptionally high startup costs that may be unique to a first unit plant will be considered to be part of the special research and development costs associated with a specific alternative, unless the high startup costs are required for all follow-on plants of the specific design.

The organization and summarization of costs will follow the recommendations of TID-7025.

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

REFUELING AND ASSOCIATED SYSTEMS

The Base Case fuel handling system is defined as a system that makes the best possible use of two Fort St. Vrain-type refueling machines under the limitations of other Base Case parameters. The 91-region core combined with a four-year fuel cycle makes the fuel handling requirement high, but the optimum use of the two machines with appropriate auxiliaries appears to be quite efficient. A study of fuel storage requirements was made. It was concluded that by lengthening the Fort St. Vrain fuel storage wells to take three machine loads of fuel each, eighteen wells appear to be adequate, and no time penalty for moving a partly empty machine is incurred.

The bottom of the wells do not extend below the lower part of the reactor core, and therefore the change involves minor changes to the storage well design.

Refueling Time Study

Work in progress on a refueling time study chart for the Base Case refueling system shows that the main crane is limiting, but crane delays do not seem to be an excessive portion of the total time as far as the study has progressed. It is intended that the refueling machine time chart for changing fixed-orifice fuel blocks will be prepared as an addition to the normal refueling chart, so that the effect of this requirement on the total downtime can be evaluated.

The use of sequential loading charts for the refueling machine and fuel storage wells has shown that it is possible to handle a ten-column region in three instead of four loads. With the use of a new fuel rack in the hot-service facility, it is possible to refuel 23 regions with 18 fuel storage wells. This rack is used to store the last two loads of new fuel and to introduce replacements for damaged reflector blocks that would otherwise be returned to the reactor. It is expected that the complexities of the Base Case refueling sequence will be documented when all the Base Case refueling charts, including that for orifice changing, are completed.

Handling System

The Base Case fuel handling system is an off-line system using two Fort St. Vrain-type refueling machines with sufficient auxiliary equipment that the main building crane, which cannot be effectively duplicated, is the pacing item. With the exception of an isolation valve positioning machine and a small mobile storage-well plug-handling cask, the individual items of equipment are identical in design to those for Fort St. Vrain. Small dimensional differences are necessary for compliance with Base-case reactor dimensions.

The Base-case reactor will be provided with equipment capable of refueling it either with new clean fuel or with reprocessed fuel when that becomes available. The physical arrangement of the fuel handling facilities is shown in Fig. 1.1 (the refueling floor plan) and Fig. 1.2 (the reactor building cross sections).

Fig. 1.1--Refueling floor plan

Fig. 1.2--Reactor building cross section

The reactor must be shut down and depressurized to permit refueling. The refueling is done through the refueling penetrations in the top head of the reactor vessel by the fuel handling machine, which reaches through the penetration to the core. Before the fuel handling machine can enter the penetration, the control rod assembly, which occupies and seals the penetration being used, must be removed and placed in a storage well. The control rod assembly is replaced with a refueling sleeve that guides and orients the fuel transfer mechanism of the fuel handling machine. Both the control rod assembly and the refueling sleeve are handled by the auxiliary transfer cask.

It should be noted that a fuel storage facility has been provided for the storage of new and spent fuel in a controlled geometry and helium atmosphere with cooling when required.

Spent fuel or radioactive reprocessed fuel is shipped by truck in a fuel shipping cask that can be set in the refueling floor preparatory to being loaded from the fuel handling machine.

Shielded gate-type isolation valves are used to shield and seal the refueling penetration, fuel storage unit, and fuel shipping cask during loading. These accommodate the auxiliary transfer cask, the fuel handling machine, or storage-well plug handling cask.

An unsealed shielding adapter supports the auxiliary transfer cask over a control rod storage well. A common fuel handling purge system provides atmospheric control in the machine, valves, and storage wells.

As shown in Fig. 1.3, the auxiliary transfer cask and fuel handling machine are moved by the main crane using a special lifting fixture. A self-propelled isolation valve positioning machine is specified to move isolation valves, and a small mobile storage-well plug handling cask is also required. Both are used to save main crane time.

Fuel Movements During Reactor Operation. Although the reactor core is not disturbed during operation, spent fuel is shipped and new fuel is received progressively in the period between refuelings.

If the new fuel is reprocessed, it is mildly radioactive and arrives in the fuel shipping cask, which is lifted up to the refueling floor and set in position. An isolation valve is placed on the cask, and the cask sealing plug is removed with the auxiliary transfer cask. The isolation valve is closed, and the interspace is purged and filled with helium by means of connections from the fuel handling purge system.

If new clean fuel is being loaded, the cask or a tank of equivalent geometry is loaded in air. The clean fuel arrives in special individual containers and is lowered into the cask using the crane and a clean fuel loading tool that has a pneumatically releasable grapple like that on the refueling machine.

The loading is done in sequence according to a computer printout from the inventory control program, which determines all fuel movements within the fuel handling equipment and reactor.

Fig. 1.3--Main crane equipment attachments

An isolation valve is set up on a fuel storage well, and the sealing plug is removed by the cask. After the valve is closed, the cask is removed. The refueling machine is then used to transfer the new fuel to its designated storage position and to transfer spent fuel from a storage unit to the shipping cask.

Spent fuel shipping operations commence after a minimum decay time in storage of about 100 days. These shipments and the new fuel receipts are scheduled to accommodate the annual reactor refueling shutdown.

Fuel Handling Operations During Shutdown. At the time of reactor shutdown, one of the isolation valves should be closing off an empty fuel storage well and another closing off the first of the storage wells holding new fuel. The control rod storage wells should be loaded with refueling sleeves and reconditioned control rod and drive assemblies, except for one that is left empty with the shielding adapter in place.

When the reactor has been depressurized, the upper cover plates are removed to gain access to the regions to be refueled. The hexagonal secondary closure cover is removed from the penetration, and the isolation valve is lowered into place, bolted down, closed, and purged. The auxiliary transfer cask is secured to the valve, purged, and used to remove the control assembly, which is then deposited in the empty control rod drive storage well. The auxiliary cask is then used to place one of the refueling sleeves in the refueling penetration, the valve is closed, and the fuel change is ready to begin.

The empty fuel handling machine is placed on the nozzle isolation valve and loaded with spent fuel. The valve is closed, and the fuel is moved to the spent fuel storage well. Two or more trips are required to take the spent fuel to the storage well, and two or more trips are required to replace it with new fuel. When the fuel has been changed, the auxiliary transfer cask is used to remove the fuel handling sleeve and install a reconditioned control rod assembly. After seal testing, the isolation valve is removed, and the penetration secondary closure and the upper cover plate are replaced.

Incremental steps of the refueling operations have been generated and integrated into a time chart. The operations covered in the chart start about 15 hr after shutdown with the reactor depressurized and cooled to refueling temperature (i.e., approximately 250°F helium inlet temperature in the core plenum). Activities such as repressurization and startup are not included in the chart.

The chart times are at best estimates of the machine-limited capabilities of the refueling system without the personal allowance, fatigue allowance efficiency factor, and learning curve correction normally applied to machine-limited time studies in the manufacturing industries. The charts do form a valid basis for equipment selection, and the times represent the lowest that can reasonably be expected without improvements in the equipment or its utilization.

The chart shows that the main crane is limiting and differs from the chart for Fort St. Vrain in that a crane utilization bar has been added.

Fuel Handling Machine

Figure 1.4 is a cross-section view of the fuel handling machine mounted on an isolation valve on top of the reactor. The fuel handling machine consists of a lead-shielded cask with a shielded valve on the bottom. This shell contains a water-cooled fuel storage rack and a fuel handling mechanism liner. The liner is a precise tube whose bore and internal rail guide can orient the fuel handling mechanism while it is in the upper part of its travel, just as the refueling sleeve guide can orient it during the lower part of its travel. The fuel handling mechanism is raised and lowered by two externally-driven suspension chains as it travels down into the reactor to pick up a fuel element and up into the cask to deposit the element in the rack.

The fuel handling mechanism consists of an upper sleeve and a lower section that move relative to each other to produce an extended length sufficient to reach the lowest fuel element in the reactor. The lower section has an inner rotating assembly that allows for the radial positioning of the attached arm, which in turn positions the grapple head, which is kept vertical by a parallel link. One electrical drive unit provides for azimuth rotation of the arm, and another positions the arm through a ball screw, crosshead, and connecting rod. The fuel grapple is engaged by springs and released by pneumatic pressure. Thus, if actuating pressure is lost, the grapple will refuse to grab an element, but will not release an element already engaged. The grapple head, although self-centering, is free to move on two axes at right angles. The head enters a displaced fuel block handling hole and by a feed-back action centers its pivot over the block. The sequencing and initial positioning of the head are computer controlled and self-correcting. Angular misalignment is provided for, and all motions are adequately interlocked to prevent maloperation.

Auxiliary Transfer Cask

As shown in Fig. 1.5, the auxiliary transfer cask is a shielded container with a simple hoist and electrically operated grapple. A shielding gate is provided at the bottom of the cask. The cask is used to handle the control rod and reserve shutdown assemblies and the refueling sleeves during the refueling operation, but it is also used for some core service and equipment maintenance operations.

Base Case Fuel Storage Facility

The Base Case fuel storage facility has 18 identical storage wells as shown in Fig. 1.6. Each of these will hold three fuel handling cask loads of spent fuel and reflector blocks. The storage is dry, and the fuel and reflector elements are stored uncanned in a helium atmosphere at a slightly negative pressure.

The internal structure of each well provides for four columns of fuel elements and one column of reflector elements, suitably restrained and cooled.

Fig. 1.4--Cross section of fuel handling machine mounted on isolation valve on top of reactor

Fig. 1.5--Auxiliary transfer cask

Fig. 1.6--Base Case fuel storage facility

Cooling is by radiation and natural-convection circulation of the helium cover gas through and around the elements, with heat being transferred to the walls of the storage compartment and thence to the cooling water that runs through tubes welded to the side of the steel walls away from the helium. The object of this construction was to have two steel walls between the fuel and the cooling water. Thus, the water from a leaking tube enters the space between the inner and outer shell and finds its way to the drain provided. Each well has two independent water-cooling circuits, one of which is adequate should the other fail. The space between the inner and outer tanks is filled with a black rock slag that acts as a heat sink and provides supplementary neutron absorbing material needed to keep the fuel well subcritical in the unlikely event that the fuel space becomes flooded.

A decontamination drain from the inner tank is sealed outside the shielding and will be connected to the radioactive liquid waste system should it prove necessary to decontaminate. A filter on the vacuum pipe prevents entry of radioactive particles into the gas waste system.

The shell of the well is designed for 5-psi internal pressure and full vacuum. Structurally, the shell is suspended so as to leave it free to expand with temperature changes, and it is restrained laterally for earthquake support. The shell, closure plug, and seals are designed to be equivalent in leak prevention to the corresponding parts of a PCRV refueling penetration. The connection to the radioactive gas waste system has an isolation valve, which is normally open except during maintenance. A relief valve is provided in a bypass line around the isolation valve to prevent buildup of excessive pressure in the tank in case the valve is inadvertently left closed.

At the start of the fuel loading operation, the sealing cover interspaces are vented through a vent valve and portable waste gas connection. The sealing cover is removed using the portable jib crane, and the isolation valve is installed. The isolation valve is then closed. The cask is moved into place to remove the fuel storage shielding plug and the isolation valve is reclosed.

The fuel handling machine is placed over the storage well, and the isolation valve is purged, checked for vacuum retention, filled with helium, and opened. The fuel transfer mechanism picks up an element and lowers it into the clearance area of the fuel storage well. The transfer mechanism arm is extended, and the fuel element is lowered in a storage slot under the guidance of protective rollers on the body of the pickup head. When the fuel element is seated in the storage well, it is released and the transfer machine head is raised to pick up another element from the fuel handling machine rack. The center column of the well is reserved for reflector blocks because they require less cooling.

When the storage well is full of fuel and associated reflector elements, the fuel handling machine is removed and the shielding plug is replaced with the cask. The well is sealed and the isolation valve is removed to the next well by a reversal of installation procedures.

Broken fuel in hexagonal cans may be stored in a well. If any fuel is broken in the well, it will be recovered by the same equipment that will be used in the reactor.

The design requirements for the fuel storage wells are:

1. The oxygen content of the cover gas shall not exceed 5%.
2. The storage wells shall safely store the spent fuel with only one of the two cooling systems operating.
3. Upon loss of one cooling system, the hottest fuel element surface temperature shall not exceed 750°F.
4. Because of the redundant supply systems, loss of all cooling water is considered incredible. In spite of this, the shells of the fuel storage units will be made of a carbon-silicon plate steel that will maintain its structural integrity at 1000°F. Thus, with nothing but natural external heat loss, the operator will have plenty of time to connect the fire water system or otherwise provide for restoration of cooling water flow.

A storage well that has survived an accident of this type is expected to be too badly distorted for normal future use.

Base Case Spent Fuel Shipping Equipment

The fuel shipping system provides a means for handling new fuel at the reactor and for handling and transporting spent fuel from the reactor to a reprocessing facility. The following major equipment is included in the fuel shipping system:

1. Spent fuel containers, which are cask liners and are removable to facilitate handling at the fuel reprocessing facility.
2. Fuel shipping casks, which are loaded at the fuel loading port in the refueling floor of the reactor building.
3. Fuel shipping trailers, which are specially designed to hold and transport the fuel shipping casks off site.

The spent fuel container and fuel shipping cask is sized to hold six 31-1/4-in.-long hexagonal fuel elements. New clean fuel is not transported in the fuel shipping cask; however, the cask may be used in the transfer of new fuel to the fuel storage wells by means of the fuel handling machine. New fuel is loaded manually into a spent fuel container within a fuel shipping cask, after which it is removed by the fuel handling machine.

The fuel shipping cask is cylindrical, with inner and outer shells of stainless steel and with depleted uranium shielding between the shells. Depleted uranium is used to reduce weight (4-in.-thick depleted uranium is approximately equal to 12-in.-thick steel in shielding value) and to satisfy accident conditions described in AEC regulations. Depleted uranium shipping casks manufactured by two United States companies have been licensed and are now in service.

STEAM GENERATOR

A draft of the steam generator design criteria was completed, reviewed, and reissued after review comments were incorporated. This criteria document is applicable to all of the steam generator designs considered in the Base Program.

The thermodynamic design and performance of the Base Case steam generator were developed. Drawings and a descriptive report were prepared and distributed for review prior to final release.

The thermodynamic design and performance of the nonreheat steam generator have been determined, and preliminary designs of penetrations for the nonreheat steam generator have been prepared. The arrangement considered thus far involves side entries into the bottom and top heads of the PCRV. A detailed review of the design concepts will be performed prior to the release of the steam generator drawings. This review will be a qualitative examination of design features and materials selection, and will also include comments as to the complexity if a complete analysis were to be performed.

CIRCULATORS

Steam-turbine-driven Circulators

The Base Case circulator sizing design summary was completed and reviewed. The design parameters for the single-stage, axial flow compressor driven by a single-stage, series steam turbine using cold reheat steam were determined and are presented in Table 1.1.

Table 1.1
DESIGN PARAMETERS

	<u>Compressor</u>	<u>Turbine</u>
Fluid	Helium	Steam
Speed, rpm	7000	7000
Inlet temperature, °F	612	724
Inlet pressure, psia	537	821
Outlet pressure, psia	550	615
Mass throughflow, lb/sec	410	284
Efficiency (including diffuser), %	80.3	85.47
Tip diameter, in.	37.38	23.35
Hub diameter, in.	24.84	19.5
No. of rotor blades	31	67
No. of stator blades	33	43
Blade centrifugal stress, psi	19,750	6080
Rotor blade bending stress, psi	4700	5000

Issue B of the design criteria document (Appendix A) for the reheat steam driven helium circulator for the Base Case was reviewed and released for information, and the layout of the circulator, with the gas reheat steam generator, was started during the quarter.

Preliminary sizing of a steam turbine following the superheater has been accomplished; this concept appears to be less efficient than the Base Case. However, further refinement of this design will be necessary since the final cycle parameters are expected to be altered somewhat.

Electric-motor-driven Circulators

Circulators for electric motor drives were sized in order to prepare design criteria and a preliminary design specification. The selection was guided by similarity considerations, which lead to compressor-type classification in accordance with the specific speed $n_s = n(Q_{in})^{1/2} (H_{is})^{-3/4}$, or the nondimensional speed $v = Cn_s$, where

n = rotational speed, rpm,

Q_{in} = inlet flow rate, ft^3/sec ,

H_{is} = isentropic head per stage, ft, and

$C = 2.634 \times 10^{-3} (\text{ft})^{3/4} (\text{ft}^3/\text{sec})^{-1/2} (\text{rpm})^{-1}$.

With the Base Case design parameters, and using six circulators, the data presented in Table 1.2 were developed.

Table 1.2
DATA ON ELECTRIC-MOTOR-DRIVEN CIRCULATORS

Motor Speed (rpm)	No. of Stages	n_s	v_s	Most Efficient Type of Machine
3550	1	165	0.435	Mixed flow
3550	2	278	0.732	Axial flow
1775	1	82.5	0.2175	Centrifugal
1775	2	139	0.366	Centrifugal

Single-stage machines are preferable because they afford a simpler layout, better part-load performance, easier control, and more economical design. Also, it is expected that 1775-rpm machines will not fit easily in the allowable space. Therefore, considerations of size and efficiency suggest the adoption of a 3550-rpm single-stage mixed-flow machine. Machines of this kind, which are intermediate between the centrifugal and the axial-flow types, are not widespread, especially in the large-size range. Information of their aerodynamic design is limited, and a considerable research and development program would be required before a satisfactory design can be achieved. The cost of such a program would not be compensated by the efficiency that could be gained through adoption of a mixed-flow design. Therefore, this design is discarded, and its neighboring types (axial flow and centrifugal) are investigated in its stead.

Preliminary sizing, carried out by the methods described in Ref. 2 for centrifugal machines, and Ref. 3 for the axial-flow machines, leads to results presented in Table 1.3.

Table 1.3
RESULTS OF PRELIMINARY SIZING

Motor Speed (rpm)	No. of Stages	Machine Type	Rotor Tip Diam (ft)	Expected Efficiency (%)
3550	1	Centrifugal	4.93	77
3550	1	Axial flow	5	79
3550	2	Axial flow	3.5	82
1775	1	Centrifugal	8.28	82.5
1775	2	Centrifugal	6.72	79

Two-stage machines were considered for the sake of comparison, but their adoption is not suggested, because of their poor part-load performance and control, and because of their complicated layout and more expensive design.

A single-stage 1775-rpm centrifugal machine shows the highest efficiency expectation based on the assumption that a suitable diffuser is installed outside the rotor. Layout considerations suggest that, with an 8.28-ft rotor tip diameter, a cavity size increase may be necessary in order to accommodate an efficient diffuser (including the flow control vanes, which will be discussed later). Alternatively, a lower-efficiency diffuser may be fitted within the cavity diameter adopted for the reference design. Either solution would penalize the 1775-rpm machine. The above reasons, together with cost considerations related to the larger machine size, suggest that the 1775-rpm design should be discarded.

The choice is narrowed down to the 3550-rpm single-stage centrifugal versus axial flow designs. The axial flow machine shows a slightly better efficiency, provided an axial-flow diffuser of adequate length is adopted. Therefore, the machine and its diffuser will occupy a larger part (expectedly, more than half) of the available cavity section, leaving little room for the electric motor. The latter will project for a considerable length (10 ft or more) above the top of the PCRV, giving rise to a more expensive construction, increasing the requirements for missile protection, and adding complication to the design of the reactor servicing equipment. On the contrary, the centrifugal machine, with its associated radial-flow diffuser, is very short and will allow housing of the electric motor inside the cavity. Besides, the centrifugal design allows a more streamlined flow path, with less turns and a smaller pressure drop. Based on these considerations, the centrifugal design was selected. The shaft power associated with this design is 9500 hp.

Control problems deserve a particular mention. With a fixed-speed drive, variable-geometry control is in order. This can be achieved by introducing variable inlet or outlet guide vanes. Inlet-guide-vane control is conducive

to a better part-load compressor efficiency, but it affects the flow path unfavorably, and makes it much more difficult to protect the vane actuating mechanism from the primary coolant environment. The latter disadvantage is very important, considering that the actuating mechanism is the most difficult part of the control device. Outlet-vane control, although somewhat less efficient at part load, allows a highly streamlined design, thus allowing better efficiency at rated operating conditions. Consequently, the concept using variable-outlet guide vanes was adopted for the electric-motor-driven Base Case circulator.

On the basis of the preliminary investigation described above, the design criteria document (DC-21-2) for the electric motor was prepared and issued for review. DC-21-2 is presented in Appendix A.

A preliminary design specification to enable an outside manufacturer to prepare and submit a proposal for a complete electric-motor-driven helium circulator unit was released for review.

A scope of work for a subcontracted conceptual design study on an electric-motor-driven circulator was prepared for submittal to potential vendors.

REACTOR PLANT ARRANGEMENT

Base Case Plant Layout

Preliminary layout drawings of the Base Case design were prepared and issued for review. At the end of the report period, the comments were being incorporated, and the final layout drawings were being prepared for release in conjunction with the plant parameters and a description of the basic arrangement.

The technology of wire-winding machine design was investigated, and a sketch was prepared for incorporation in a criteria sheet. This sheet shows that all essential features of proprietary wire-winding machines could be incorporated in a unit that would work in a 5-ft annulus. In using this criteria sheet for plant layout, it is assumed that the small amount of mechanical design work required to build such a machine under a licensing agreement would be well justified by the efficient plant layout it makes possible.

Steam Piping

A compilation of policy statements on piping system classification and missile protection in the Fort St. Vrain Generating Station Preliminary Safety Analysis Report was made in order to provide criteria for the missile protection aspects of current piping layout studies. The most significant policy statements used were related to flailing steam lines and tornado-borne missiles. A missile shielding concept drawing was completed and its configurations incorporated in the plant layout drawings.

Analysis of secondary coolant piping for main steam, hot reheat, cold reheat, and feedwater were performed, including the Base Case heat balance

parameters and the missile protection requirements. These analyses confirmed the design with respect to allowable stresses and forces.

Separate Containment

Pressure, temperature, and leakage rate criteria were prepared, and a preliminary containment building design program specification was produced for review and comment. This specification incorporated a drawing which gives limiting dimensions for the use of the engineer constructor who will develop the containment building design. These data will be published during the next report period.

Equipment Installation

Plan view and section view drawings were prepared which showed the use of customized rented riggers hoisting equipment for the installation of steam generators. The availability of these drawings permitted a start to be made on a description of the mechanical aspects of the major equipment installation and service operations provided for in the Base Case plant design.

SYSTEM ANALYSIS

Fuel element design and fuel cycle data were calculated and the Base Case design parameters, with the exception of reactor physics data, were compiled during the report period. These data are presented in the design criteria document, DC-1-1, Issue A, (Appendix B).

The parameters that are listed in DC-1-1 are based largely on the selection of system characteristics that are discussed below.

Primary Coolant System

The fiscal year 1969 Task I effort will not include system optimization. As a result, plant cycle characteristics were selected at the start of the program that are not only technically acceptable, but also economically advantageous, since they provide the basis for the component, steam cycle, and plant arrangement assessments. In this respect, the primary coolant (helium) pressure is one of the more significant parameters.

The multicavity arrangement recommended as the reference design for the 1000-Mw(e) HTGR in Ref. 4 was based on ground rules that set the primary coolant pressure at 700 psia. In a subsequent study (Ref. 5), an economic analysis indicated a cost saving equivalent to \$3.52 per installed kilowatt if the primary coolant maximum pressure were reduced to 550 psia and the cold helium temperature were reduced to 605°F by reducing the feedwater temperature to 315°F. It was determined that higher fuel cycle costs resulting from a degradation of about 0.7 percentage point in plant efficiency were offset by a reduction in amortized capital costs. The study resulted in the selection of 550 psia as the reference pressure for the 1000-Mw(e) design, and a subsequent effort (Ref. 6) supported the selection.

In view of the economic considerations discussed above, 550-psia helium pressure will be used for the fiscal year 1969 Task I effort. This selection is also based on the following factors:

1. The use of 550 psia has no apparent effect on the conclusions reached in Ref. 4 regarding plant arrangements (i.e., the over-all plant configuration is somewhat insensitive to moderate changes in operating pressure).
2. The circulator design requirements are approximately equivalent for the 700-psia and 550-psia cycles because of the helium density and mass flow conditions of each cycle.
3. The use of 550 psia, rather than 700 psia, has no impact on the results of the Base Program refueling studies performed during fiscal year 1968, since the pressure vessel of the fuel handling machine is based on shielding rather than pressure requirements.
4. The Fort St. Vrain core height appears to be compatible with the operating conditions at the reduced pressure level, which permits the use of unchanged Fort St. Vrain control rods and drives as well as the refueling machine.

Secondary Coolant System

Heat-cycle Parameters. The purpose of the fiscal year 1969 Task I study is not to optimize the steam/water heat-cycle parameters, but to examine the effects of varying the primary coolant system components, circulator drives, and reactor plant arrangements. Therefore, the objective is to set a basic steam cycle consistent with the HTGR concept and providing reasonable cost and technical performance. From these premises, the more important of the steam/water-cycle parameters selected for the Base Case are summarized below:

Turbine inlet steam conditions	2400 psig, 1000°/1000°F
Turbine arrangement	TC6F-31 in.
Final feedwater temperature	330°F
Number of feedwater heaters	4
Design circulating water temp	60°F
Condenser pressure	2 in. Hg abs

The reasons leading to these selections are presented in the sections that follow.

Turbine Inlet Steam Conditions. A preliminary assessment (Ref. 7) of 1000°/1000°F and 1050°/1050°F steam cycles at 3500 psig has shown that the 1050°/1050°F steam cycle offers little possibility of economic gain over the 1000°/1000°F cycle. Main and reheat steam have therefore been set at 1000°/1000°F for the Base Case.

As mentioned in Ref. 7, a precise cost comparison of 3500 psig versus 2400 psig HTGR steam generators has not been made; however, it has also been determined that in order for the 3500-psig cycle to be economically attractive, the additional cost for the 3500-psig steam generators would have to be below \$2 million. Preliminary information indicates that the cost difference is likely to be at least \$2 million, and it appears that there is little economic incentive to go to supercritical pressure on HTGR plants up to 1000-Mw(e)

capacity. On this basis, 2400 psig was selected as a reference pressure level for the present Base Case.

Turbine Arrangement. As a result of developments in turbine-generator manufacture in the last few years (such as water-cooled generator stators, longer turbine shafts with capability for three low-pressure turbines in tandem, and increases in 3600-rpm last-stage blade heights), a tandem-compound turbine-generator of 1000-Mw(e) capacity has become practicable and more economical than a cross-compound machine of the same rating. This is especially true of HTGR power plants with their relatively low heat rejection compared to water reactor plants. A TC6F-31 in. machine is therefore selected for the Base Case. The 31-in. last-stage blade height signifies a specific manufacturer (Westinghouse), but the conclusions of the study will not be significantly affected by substituting an equivalent machine of another manufacturer (e.g., General Electric's TC6F-33-1/2 in.).

Final Feedwater Temperature. The final feedwater temperature required is approximately a function of the cold helium temperature and is set at 330°F to suit the cold helium temperature selected (viz. 605°F). This is consistent with the nearest extraction pressure available from the TC6F-31-in. turbine, yielding at the same time a satisfactory LMTD for the steam generator. No high-pressure feedwater heaters are required; the cycle consists of three low-pressure feedwater heaters and a direct-contact deaerating heater with a deaerator storage tank.

Cooling Water Temperature and Turbine Exhaust Pressure. In Section 110 of Ref. 1 (TID-7025), the following temperatures and exhaust pressure for nuclear power plant studies are suggested:

Design cooling water temp.	57°F
Avg. max. cooling water temp.	75°F
Turbine exhaust pressure	1-1/2 in. Hg abs

Previous studies on 1000-Mw(e) HTGR plant design at Gulf General Atomic have been based on 60°F design cooling water temperature. Based on this, the heat-rejection optimization effort has yielded a 2-in. Hg abs exhaust pressure as being optimum for the HTGR plant operating conditions. The average maximum cooling water temperature has been assumed to be 73°F for the design of plant auxiliary exchangers. Since these temperatures are within a few degrees of TID-7025 recommendations, and have no significant effect on the fiscal year 1969 Task I evaluations of the primary system and components, 60°F, 73°F, and 2 in. Hg abs have been chosen to be the respective parameters for the Base Case.

Remainder of the Heat Cycle. The remainder of the heat cycle conforms to the usual central station design practice. The hot reheat steam inlet pressure is set at 544 psia, in line with the optimum generally accepted for the 2400-psig 1000°/1000°F cycles by turbine manufacturers and users. The boiler feedwater pumping arrangement used consists of two 50% pumps each driven by a turbine taking its steam supply from IP-LP crossover point and exhausting to an auxiliary condenser. The choice of the auxiliary condenser somewhat relieves the loading on the main turbine exhaust end and also serves the shutdown heat-rejection function should the main condenser not be available under abnormal conditions.

Table 1.4

BASE CASE CYCLE PARAMETERS FOR CASE BP1

Over-all Plant Performance

Gross turbine generator output, Mw(e)	1,016.5
Net plant output, Mw(e)	1,000.0
Net plant efficiency, %	39.39
Net plant heat rate, Btu/kw-hr	8,663

Primary Coolant System

Reactor gross thermal power, Mw(t)	2,538
Helium flow through circulators, lb/hr	8,862,000
Helium temp. at circulator inlet, °F	612
Helium temp. 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 SystemTurbine Generator

Turbine throttle pressure, psia	2,414
Turbine throttle temperature, °F	1,000
Turbine throttle flow, lb/hr	6,505,900
HP turbine exhaust pressure, psia	846
IP turbine inlet pressure, psia	544
IP turbine inlet temperature, °F	1,000
IP turbine steam flow, lb/hr	6,458,900
LP turbine backpressure, in. Hg abs.	2.00
Turbine arrangement	TC6F-31 in.

Regenerative Feedwater Cycle

Feedwater flow, lb/hr	6,523,100
Final feedwater temperature, °F	330
Number of feedwater heaters	4

Helium Circulator Turbine Drive

Inlet pressure, psia	821
Inlet temperature, °F	724
Outlet pressure, psia	624
Approximate 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
Reheater outlet pressure, psia	570
Reheat outlet temperature, °F	1,001
Reheater pressure drop, psi	45

The boiler feed pump turbine has a shaft extension at the end away from the boiler feed pump, to which a booster pump is coupled, so that the boiler feed pump gets adequate NPSH under all conditions without the necessity of locating the deaerator at an extremely high elevation in the plant.

Base Case Cycle Parameters

The cycle operating parameters given in Table 1.4 for the Base Case were based on the discussions presented above and on a calculated heat balance. As the power plant design is developed, the heat balance will be updated, if necessary, to reflect revised predicted component performance characteristics. Figure 1.7 is a heat balance diagram showing the gas reheat steam cycle conditions.

Figure 1.8 is a schematic diagram of the primary coolant circuit showing the required flow rates and estimated seal leakage flows and temperature and pressures at principal points. Pressure loss allowances are discussed in detail in the next section. The helium design conditions for the circulators are 537 psia and 612°F at the inlet and 550 psia and 625°F at the discharge.

Primary Circuit Pressure Losses

Primary circuit pressure-loss allocations were made in order to complete the Base Case heat balance described above. Additional detail was developed to support the allocations made in the heat balance and also to provide for later allocations to be made in the reactor-only PCRV case.

The principal elements of the Base Case pressure losses are given in Table 1.5.

Table 1.5
BASE CASE PRESSURE LOSSES

<u>Part of Primary Circuit</u>	<u>Design Pressure- loss allowance (psi)</u>
Core (including about 2 psi allowance for fixed orifices)	5.2
Steam generator tube bundle	4.2
Ducts, including plenums	2.0
Contingency allowance	<u>1.6</u>
TOTAL	13.0

The allowances are based on design projections where details are not yet developed, such as core supports and similar restrictions in the helium flow path.

Fig. 1.7--Heat balance showing gas reheat steam cycle conditions

Fig. 1.8--Primary circuit parameters

The pressure losses are not expected to change if a separate containment is placed over the PCRV, enclosing both the reactor core and steam generators. For the reactor-only PCRV, however, the pressure-loss allocation has been increased as shown in Table 1.6.

Table 1.6
HELIUM PRESSURE LOSSES FOR REACTOR-ONLY PCRV

<u>Part of Primary Circuit</u>	<u>Design Pressure- loss Allowance (psi)</u>
Core (including approx. 2 psi allowance for fixed orifices)	5.2
Steam generator tube bundle	5.0
Steam generator shell annulus and related parasitic losses	1.0
Concentric ducts, plenums, and extra flow pass in the PCRV	2.5
Contingency allowance	<u>1.8</u>
TOTAL	15.5

A schematic flow diagram of the arrangement visualized is shown in Fig. 1.9. The pressure losses in such an arrangement are expected to be affected, relative to the Base Case, as discussed below.

The core pressure is assumed to remain unchanged at 5.2 psi.

The steam generator tube bundle will very likely require a slightly smaller frontal area than the Base Case to keep the steam generator shell within a diameter that can be shipped, considering that the outside shipping dimension will be larger than the tube bundle by twice the shell thickness plus twice the shell annulus width. Earlier design studies on steam generators with steel shells indicated the desirability of increasing the pressure loss allowance for the tube bundle from 4.2 psi to 5 psi. This corresponds to about a 6% reduction in frontal area relative to the Base Case. (A 1% decrease in frontal area requires an increase of 1% in the length of the tube bundle and the pressure loss increases by nearly 3% as a result.)

Since the cooled gas will make a return path through the steam generator shell annulus (in part, to keep the shell cool), an allowance of 1.0 psi is added for the annulus including related parasitic losses, using extrapolation from experience with the earlier design studies.

The duct between the PCRV and the steam generator will be somewhat longer, but the effect on pressure loss of lengthening a straight duct is small compared to that of expansions, contractions, and changes in direction of the gas. The cooled gas will also make a return pass from the concentric

Fig. 1.9--Flow diagram for reactor-only PCR (flow circuit is schematic, duct geometry and other details can vary)

duct through the PCRV annulus to the top of the reactor core. With the present preliminary knowledge of the geometry, the combined pressure loss allowance for these effects is set at 2.5 psia (compared to 2.0 psi for the Base Case).

The contingency allowance of 1.6 psi for the Base Case has been increased to 1.8 psi for the reactor-only PCRV, the same percentage of the total as in the Base Case.

Nonreheat Cycle Parameters

The cycle parameters, which were calculated for a nonreheat plant with a reactor thermal power equal to the Base Case plant, are listed in Table 1.7.

The turbine generator for the nonreheat cycle is a CC4F cross-compound machine with 3600 rpm HP and four-flow 1800 rpm, 43 in. LSB LP sections, compared to the tandem-compound unit of the Base Case. LP blades of 3600 rpm are not commercially available for turbines with high exhaust moisture occurring in the nonreheat cycle (16% nominal compared to approximately 9% of the Base Case). The net plant efficiency for the nonreheat cycle is 37.8% against 39.4% of the Base Case, and the net output 959.5 Mw versus 1000 Mw for equal reactor thermal power. The helium and steam flows calculated also correspond to the same reactor thermal power as the Base Case. When making a cost comparison, a capability charge for the differential electrical power will be applied to arrive at final cost figures in terms of plants of equal electrical size.

The circulator turbine, like the Base reference cycle, is located in the high-pressure turbine exhaust line, and the required helium flow is equal to the Base Case circulation rate for the same reactor thermal output.

Figure 1.10 is a heat balance diagram showing the steam cycle conditions. The primary circuit conditions are shown on Fig. 1.8.

Condensing Turbine for Circulator Drives

An investigation was made into possible interactions of a condensing steam turbine setup with the HTGR power plant in areas of equipment rating, cost and configuration, efficiency and cycle performance, mounting and missile protection considerations, shielding, and possible effect on startup and shutdown concept.

A "condensing" turbine is generally defined as one exhausting to a pressure less than 15 in. Hg abs. The most common turbine exhaust connection for a condensing turbine is to the main condenser. The exhaust pressure is 2 in. Hg abs at the condenser for the Base Case, or 2.5 in. Hg abs at the circulator turbine exhaust, allowing for a 0.5 in. Hg frictional loss in the exhaust ducts. Figure 1.11 shows the location of the turbine in the cycle as selected for the Base Program investigation.

The approximate ratings for each of the six condensing turbines were selected on the basis of an estimate of primary and secondary coolant system requirements and capabilities for the Base Case configuration. These ratings are listed in Table 1.8.

Table 1.7

NONREHEAT CYCLE PARAMETERS FOR CASE BP6

Over-all Plant Performance

Gross turbine generator output, Mw(e)	976.4
Net plant output, Mw(e)	959.5
Net plant efficiency, %	37.80
Net plant heat rate, Btu/kw-hr	9.028

Primary Coolant System

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

Secondary Coolant SystemTurbine Generator

Turbine throttle pressure, psia	2,414
Turbine throttle temperature, °F	1,000
Turbine throttle flow, lb/hr	7,552,300
HP turbine exhaust pressure, psia	759
IP turbine inlet pressure, psia	544
IP turbine inlet temperature, °F	645
IP turbine steam flow, lb/hr	7,509,252
LP turbine backpressure, in. Hg abs	2.00
Turbine arrangement	CC4F-43 in. -3600/1800 rpm

Regenerative Feedwater Cycle

Feedwater flow, lb/hr	7,569,500
Final feedwater temperature, °F	330
Number of feedwater heaters	4

Helium Circulator Turbine Drive

Inlet pressure, psia	737
Inlet temperature, °F	697
Outlet pressure, psia	579
Approximate 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

Fig. 1.10--Heat balance showing nonreheat steam cycle conditions

Fig. 1.11--Condensing turbine drive for helium circulators

Table 1.8
CONDENSING TURBINE RATINGS

Type	Straight condensing
Power output	7333 kw
Inlet conditions	167 psia, 714°F
Exhaust pressure	2-1/2 in. Hg Abs
Rated speed	6500 rpm ^a
Automatic speed variation range	5% to 105%
Over-all efficiency at rated point, referred to isentropic	74%
Estimated steam rate	11.75 lb/kw-hr
Estimated steam flow	86,000 lb/hr

^aAny value between 6000 and 7000 acceptable.

A detailed cycle heat balance was performed in support of the investigation of the condensing turbine. It was determined that with the crossover flow between the intermediate- and low-pressure turbines smaller for the Base Case, an over-all saving in heat rate of 30 Btu/kw-hr, principally from a reduced exhaust loss, is realized.

Cost figures were prepared that included the evaluation of the reduced heat rate as well as the variations in capital investment that result in the investigation of the use of the condensing turbine. These cost figures are summarized in Table 1.9 below:

Table 1.9
COST DIFFERENTIAL FOR CONDENSING TURBINES
(Direct Costs)

Cost Additions (\$)	Cost Savings (\$)
Multistage condens- ing turbines 1,915,000	Omit turbines in series with reheater 600,000
Extra for T-G set ... 53,000	Reduced aux. boiler capacity 160,000 (max)
EHC system <u>240,000</u>	Incremental capability .. 175,000
Total 2,208,000	Equivalent fuel cost <u>215,000</u>
	Total 1,150,000

The multistage condensing turbine cost is fairly closely known from manufacturers' price data. Even some allowance for reasonable perturbations in other costs will not significantly change the relatively massive net penalty of approximately \$1 million for choosing the condensing turbine drive in place of the present integral turbine drive of the Base Case (in series with cold reheat). In addition, the uncertainties in making the commercial horizontal multistage condensing turbines adaptable to the HTGR configuration, and the possible extra costs of modifying the circulator bearing and seal arrangement, and of meeting the relatively more severe shielding and missile protection requirements, rule out any promise that any economic benefit can accrue from continuing a detailed evaluation of the condensing turbine drives.

It was concluded that the condensing turbine drives for helium circulators do not offer an incentive for exploration in depth. This is, however, not to say that alternative cycle locations for a parallel turbine (i.e., one taking only a part of the main turbine steam flow) may not hold promise. For reasons of drive compactness and economy of auxiliary steam requirements, it would be advantageous if the maximum possible amount of work per pound of steam is extracted (consistent with efficiency) from a single-stage turbine (straight-single or velocity-compounded stage). This implies a noncondensing parallel turbine, and it is indeed believed that an investigation of such a turbine located at a suitable place in the cycle has potentially more to offer to the HTGR technology than a detailed study of the condensing turbine. By way of illustration, Fig. 1.12 shows a possible location (in dotted line).

PCRV DESIGN

PCRV Geometric Factors

In addition to cycle parameters, geometric factors were established to form the basis for the Base Case PCRV design assessments. These are listed in Table 1.10.

Table 1.10
BASE CASE PCRV GEOMETRICAL FACTORS

Number of steam generators	6
Number of circulators	6
Number of refueling penetrations	91
Number of control rod drives	91
Refueling penetrations triangular pitch	37.6 in.
PCRV inside diameter (liner)	40 ft 3 in.
PCRV inside height (liner)	44 ft 8 in.
Steam generator cavities inside diameter	12 ft 9 in.
Effective core diameter	30.6 ft
Core stack height	15.6 ft

It should be noted that subsequent Task I assessments may dictate minor dimensional changes; however, the number of steam generators, circulators, and control rod drives, and the number and triangular pitch of the refueling penetrations will be kept constant.

Fig. 1.12--Circulator turbines in parallel with reheat and intermediate pressure turbine

PCRV Design Criteria

The work on the PCRV safety and design philosophy leading to an updated PCRV design criterion is continuing. The work covers the requirement for linear elastic PCRV response to design loads (which turns out to be a serviceability criterion rather than a safety criterion), and the evaluation of the significance of stress predictions due to prestress, temperature, and pressure with respect to the structural safety. A finite-element analysis is being prepared for a typical multicavity PCRV horizontal cross section.

Investigation of the shielding requirements around the core confirmed the preliminary core cavity liner ID of 40 ft 3 in. for the 601-column HTGR core. The findings were based on estimates of acceptable limits for fast-neutron exposure of the liner, gamma exposure of the closest (2 ft) tendons, and nuclear heating of the PCRV.

STRUCTURES

Site Conditions

In TID-7025 the AEC has specified a hypothetical site for use when factual data for a specific proposal are not available. The pertinent portion of this document is Section 110. In order to perform the structural design assessment portion of Task I in the most meaningful manner, and also to take maximum advantage of previous HTGR system analysis experience at Gulf General Atomic, the site characteristics described in the reference were adopted with the exception of the seismic conditions, wind conditions, and the turbine back pressure discussed below.

Seismic Conditions. The AEC hypothetical site location is in a Zone 1 area as designated by the Uniform Building Code. This designates a design earthquake with a maximum ground acceleration of 0.08 g. Figure 1.10 of TID-7024 (Ref. 8) shows the Seismic Probability Map of the United States as developed for the 1958 Uniform Building Code. It should be noted that most of California is located in Zone 3 within maximum ground acceleration for the design earthquake of 0.33 g. Studies have shown that the HTGR plant layout may become quite dependent upon the seismic site conditions. It was believed that the most meaningful design effort within the Base Program would be accomplished by assuming a site location in the Zone 2 area. In this manner, the Base Program HTGR plant design will be representative of most site locations in the United States.

With the hypothetical site located in a Zone 2 area, the plant design should be made for a design earthquake with a maximum ground acceleration of 0.16 g. This value is obtained from paragraph 4.12 of TID-7024. The maximum earthquake should be assumed to have 0.25 g maximum ground acceleration. The 0.25 g value is representative of the highest loadings used for six presently planned or existing reactor sites in Zone 2.

With the above maximum ground accelerations, the response spectra of Figs. 1.19 to 1.24 of TID-7025 can be modified and used as the basis for the plant seismic design.

Wind Conditions. TID-7025 states that "100-mph winds were recorded at the hypothetical site." The AEC also requires that a 300-mph tornado be considered for all plants east of the Rocky Mountains. All structures and equipment necessary for safe shutdown must be able to resist such a tornado.

The Base Program plant designs will be based on 100-mph winds with a 33% increase of allowable stresses and the AEC requirement for a tornado of 300 mph horizontal vortex velocity and 60 mph translational velocity will be observed. Plastic design will be allowed. Siding not necessary for protection of Class I equipment will be allowed to blow out.

It is believed that the plant does not need to be designed for a Design Tornado as it was for the PSC plant. The design for 100 mph wind will guarantee that substantially higher wind loads can be resisted by the structure without causing major damage.

Turbine Condensers. TID-7025 states that the turbine condensers "shall be designed to maintain a back pressure of 1-1/2 in. Hg absolute, based on a 57°F circulating water temperature ..." The turbine condensers will be based on 2 in. Hg absolute with a circulating water temperature of 60°F, for the reasons stated in the section on the secondary coolant system.

Cooling Water Temperature. TID-7025 states that "the North River average maximum temperature is 75°F and the average minimum is 40°F." An average maximum temperature of 73°F will be used, for the reasons stated in the section of the secondary coolant system.

Structural Design

Efforts to define the seismic problems associated with the PCRV support and to develop a feasible support scheme were continued during the report period. The largest effort was spent on devising a PCRV low-frequency support that produces core seismic horizontal accelerations of less than 0.2 g.

A preliminary study of support concepts for a large PCRV led to the conclusion that a monolithic, or any other stiff support concept is not feasible, because of the restrictive acceleration criterion. The use of controlled ground flexibility to reduce the stiffness of a support was investigated, and it was found that when the required flexibility is attained, soil strength becomes a problem. Four unconventional flexible supports, namely cables, bearings, pinned columns, and elastomeric, were found to be feasible but possibly difficult or costly to manufacture.

The column support concept, because of the strong interdependence of stiffness and strength, presents a difficult design problem. A parametric study was employed to obtain several feasible designs for both concrete and steel columns. All of these designs proved to be cumbersome, with the exception of a "wire brush" concept that uses a large number of small-diameter columns. Work is being continued in order to further investigate several support concepts that appear feasible.

The Design Criteria for Structures, DC-70, which is included as Appendix C, was reviewed and issued during the report period.

SYSTEMS DESIGN

Reviews were completed and comments received on the Base Case primary coolant system piping and instrumentation diagrams (PI-21-1, 21-2, 21-3), and on the process flow diagrams (PF-21). Incorporation of the comments was in progress at the end of the report period.

Process flow and piping and instrument diagrams were prepared for the secondary coolant system. These drawings and a system description are being issued for review.

Work on the reactor plant cooling water system (System 46) was started.

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Task III
ANALYTICAL METHODS

HEAT-TRANSFER CODES

Work continues on the heat-transfer codes RAT-3D and TAC, with the effort aimed at cleaning up the codes and documentation of the codes at this point in their development.

A draft of the TAC User's Manual has been prepared. This manual gives detailed instructions on the basic use and many specific applications of the TAC heat-transfer code. After the draft has been used internally and some comments have been received, it will be published as a formal report for external use. An analytical solution to a three-dimensional, transient heat-transfer problem was obtained to use as a sample problem for the RAT-3D User's Manual.

The printout modifications (which include a pictorial print of block boundaries for use as a temperature overlay, an improved print of the coolant and surface temperature information, a complete listing of the input cards, and a listing of the punched card output) are complete and debugged for both TAC and RAT-3D.

In the original version of TAC and RAT-3D, the evaluation time for surface temperatures was inconsistent with the evaluation of the internal nodal temperatures. The program revisions to correct this inconsistency are complete and debugged for TAC and are nearly complete for RAT-3D. To make room for the additional programming in this revision and for other minor alterations, the subroutine LINK was modified in both RAT-3D and TAC. The latter was done primarily to keep the codes similar.

A programming error was found in the TAC code concerning radiation across gaps in the θ -coordinate direction, and it was corrected.

An instability in the TAC and RAT-3D codes that often occurs in problems having radiation across an internal coolant channel was initially identified. A continued investigation of the problem has shown that the condition develops because of limitations in the algorithm for evaluating the conductance between a solid node and a coolant node when there is radiation. It appears that current revisions in the code, which are nearly complete, will provide a satisfactory correction to this problem.

CORE THERMAL PERFORMANCE CODE (FLAC)

Work on the core thermal performance code, FLAC, commenced during the reporting period after being inactive during the previous two quarters. The

objective of this task is to develop a computer code for the analysis of flow distribution and thermal performance for stacked cores. The unique feature of the code is the solution of the flow distribution problem, which allows for crossflow between the primary coolant flow paths. Previous work had developed the code to a point at which a form of point relaxation appeared to be an attractive iteration scheme for solving for the pressure and flow distribution in the flow network.

During this reporting period, the point relaxation iteration method was investigated in some detail to obtain quantitative results on the rate of problem convergence. The method of solution is derived by first linearizing the equation expressing pressure loss in each individual branch and then satisfying mass flow balance at each node. The solution is analogous to satisfying Kirchoff's Laws in an arbitrary flow network. The resulting equations are a set of simultaneous linear equations expressing node pressure as a function of the adjacent node pressures and the corresponding branch flow resistances. The basic procedure is to solve for the pressure at each node using previously computed values for the adjacent node pressures and branch flow resistances.

In applying the point relaxation technique, two alternatives are available for correcting the node pressures and branch flows at each iteration:

$$P_{\text{new}} = P_{\text{old}} + \lambda(P'_{\text{new}} - P_{\text{old}}) \quad , \quad (1)$$

$$W_{\text{new}} = W_{\text{old}} + \lambda(W'_{\text{new}} - W_{\text{old}}) \quad , \quad (2)$$

where P and W are the node pressure and branch flow, respectively, and λ is a relaxation factor.

Several test problems were solved using the above two relaxation methods with different values for λ to compare the rate of convergence. The results showed clearly that flow relaxation, Eq. (2), is superior to pressure relaxation, Eq. (1). For a given number of iterations and using the optimum value of λ for each method, the degree of convergence using flow relaxation was better by a factor of 10 than for pressure relaxation. In addition, the rate of convergence using flow relaxation was not very sensitive to the value of λ . The optimum value of λ was about 0.25, and a variation of a factor 1.5 from the optimum caused only a 10% change in the rate of convergence. In contrast, the rate of convergence using pressure relaxation was strongly dependent on the value of λ . This characteristic is undesirable, since the optimum value of λ may vary from one problem to the next. It was therefore decided to adapt the flow relaxation method into the FLAC code.

Task IV
FISSION-PRODUCT MECHANISMS

MATHEMATICAL TREATMENT OF FISSION-PRODUCT TRANSPORT

SLIDER Code

Additional SLIDER code calculations have been made for postactivation fuel particle anneals to describe (1) the behavior of fission products when diffusion rates in the coating and kernel are similar, (2) the effect on the release of a buffer layer between the kernel and coating, and (3) the perturbations on releases caused by fission-product recoil. Calculations of this type have been described in a previous quarterly report (GA-8662). Analysis of these recent calculations is in progress.

FIPER Code

The one-dimensional FIPER code (FIPER I) is undergoing some technical modifications to produce a fast and flexible production code for use in calculating the simultaneous releases of different metallic fission products for large HTGR systems.

FIPER I has several advantages over FREVAP-8, the production code developed earlier for use in predicting metallic-fission-product release in HTGR systems. FREVAP-8 tends to overestimate release values. Two reasons for this are (1) it does not treat transient behavior adequately and (2) it uses a release-constant concept rather than a condensed-state diffusion concept, as in the FIPER code, for calculating the release from particles.

The FIPER code is a mathematically precise description of the release of metallic fission products from an HTGR fuel element. It treats complex aspects of the problem, such as transient behavior. The two-dimensional code (FIPER II), now under development, will describe axial transport as well as radial transport. The release of Sr^{90} calculated with FREVAP-8 under typical reactor conditions of temperature and power density distribution was found to be at least an order of magnitude larger than the release calculated using FIPER I. Furthermore, the computer time was smaller for FIPER.

Attempts are being made to provide the FIPER I one-dimensional code with features that will be available when the FIPER II two-dimensional code is developed. For example, some of the radioactive fission products in the coolant gas of an HTGR exert a backpressure on the fission products in the structural graphite. In FIPER I release calculations, this backpressure effect is neglected. Therefore, the calculated release is too large. To approximately account for the backpressure effect, the one-dimensional release currents calculated for horizontal reactor segments are coupled via the coolant channel. Thus, a two-dimensional release is simulated. This procedure,

however, neglects vertical diffusion in the structural graphite of the reactor core. The numerical analysis of the backpressure effect is completed and the corresponding equations are programmed.

Experimental Test of the FREVAP-8 Code

In the FREVAP-8 (Ref. 1) calculation of metallic fission product release from HTGR fuel elements, it is assumed for simplicity that the migration of fission-product metals such as strontium and barium through the fuel-free graphite that separates HTGR fuel from its helium coolant can be represented by steady-state diffusion equations. The external boundary conditions contains the metal adsorption isotherms and mass transfer coefficients derived using known heat-transfer correlations. These and other more detailed assumptions were tested by comparing observed and calculated releases from experimental fuel elements irradiated in the PLUTO loop at Harwell for the Dragon Project (Refs. 2, 3) and in the General Atomic GAIL loop in GETR at Vallecitos (Ref. 4).

Release rates for the fuels and diffusion coefficients used in the calculations were derived from measurements made on components of these and other experiments, both in and out of pile. The adsorption isotherms used were those determined at Gulf General Atomic for a nuclear grade graphite (TS-688).

Table 4.1 describes the in-pile loop experiments and Table 4.2 gives the results of the comparisons between experiment and calculation. Analyses of the coolant gas showed that the rates of release of Kr^{89} , Kr^{90} , and Xe^{140} were insufficient to account for the Sr^{89} , Sr^{90} , and Ba^{140} found in the PLUTO coolant circuits.

Table 4.1
DESCRIPTION OF IN-PILE LOOP EXPERIMENTS

Exp.	Fuel Coating ^a	Irradiation Time (days)	Peak Fuel Temp (°C)	Peak Surface Temp (°C)	Fuel-free Zone Thickness, L (mm)	Coolant Flow Rate (g/sec)
PLUTO 8	PyC/SiC/PyC	173	1400	1350	1.25	18
PLUTO 15	PyC/PyC	162	1370	1320	1.25	18
GAIL IV	PyC/PyC	400	1450	1100	≤6.35	35

NOTE: L^2/D for a PLUTO element is greater than 1/23 that for GAIL IV; hence, a PLUTO element is that much closer to steady state. Alternatively, a PLUTO element represents the behavior of an element with a 6-mm fuel-free zone after more than 10 years of irradiation, other things being equal.

^aPyC = pyrocarbon.

Table 4.2

OBSERVED VERSUS FREVAP-CALCULATED Sr AND Ba ACTIVITY
RELEASE FOR PLUTO 8, PLUTO 15, AND GAIL IV
(in curies)

Nuclide ^a	Experimental Data	Based on FREVAP
PLUTO 8		
Sr ⁹⁰ , FFZ	2.7x10 ⁻²	8.4x10 ⁻³
Sr ⁹⁰ , to coolant	7.3x10 ⁻³	6.9x10 ⁻³
Sr ⁸⁹ , to FFZ	1.2	3.4x10 ⁻¹
Sr ⁸⁹ , to coolant	3.2x10 ⁻¹	2.7x10 ⁻¹
Ba ¹⁴⁰ , to FFZ	5.2x10 ⁻²	3.4x10 ⁻²
Ba ¹⁴⁰ , to coolant	2.2x10 ⁻³	1.8x10 ⁻⁵
PLUTO 15		
Sr ⁹⁰ , to FFZ	Not determined	7.0x10 ⁻¹
Sr ⁹⁰ , to coolant	Not determined	6.5x10 ⁻¹
Sr ⁸⁹ , to FFZ	4.4x10 ¹	3.6x10 ¹
Sr ⁸⁹ , to coolant	3.5x10 ¹	3.3x10 ¹
Ba ¹⁴⁰ , to FFZ	4.8	9.2
Ba ¹⁴⁰ , to coolant	5x10 ⁻²	5.0
GAIL IV		
Sr ⁹⁰ , to FFZ	4.7	6.4x10 ^{1b}
Sr ⁹⁰ , to coolant	2.8x10 ⁻²	5.2
Ba ¹⁴⁰ , to coolant	(3 to 12)x10 ⁻⁵	5.8x10 ⁻¹¹

^a-FFZ = fuel-free zone; to FFZ signifies the total quantity entering zone.

^b-Based on a conservative (high) estimate of "release constants." A calculated release of 2.4 C is obtained using release data from in-pile experiments.

The FREVAP-8 calculation for the GAIL element yielded Sr releases that were very much higher than those observed. Here, neglect of transient diffusion from strontium has resulted in a calculated strontium release 100 times greater than that observed. For the short-lived Ba¹⁴⁰, the situation is reversed, because the calculation predicts that effectively all the Ba¹⁴⁰ will decay during its passage through the thick fuel tube (as it is in fact found experimentally), whereas in reality there is a small release of Ba¹⁴⁰ as Xe¹⁴⁰. Confirmation that Xe¹⁴⁰ release can account for observed Ba¹⁴⁰ has been obtained from GAIL and other experiments with similarly thick fuel tubes.

It is concluded that estimates of release from HTGR fuel elements, in general, were quite conservative. Use of the FIPER method of calculation discussed in the previous section, which takes into account transient behavior, would eliminate economic penalties from overdesign or the overestimation of hazards associated with Sr⁹⁰ in the coolant.

Calculation of Fission-product Transport in Porous Graphite

As described in an earlier quarterly report (GA-8530) an equation based on a treatment by Fisher (Ref. 5) and modified by Blackburn and Brown (Ref. 6) was used to calculate concentration profiles for the diffusion of strontium in graphite by simultaneous bulk and pore diffusion. The model was simplified from a two-dimensional system to a one-dimensional system, thus affording ease in manual calculations. The simplification yields a conservative equation that predicts higher transport by a maximum of about 20% above that of the two-dimensional case. For increasing times, the agreement becomes increasingly better.

It is of interest to apply the equation to a one-dimensional model consisting of bulk graphite that is characterized by a low diffusivity, D , and a pore of width, W , that is characterized by a high diffusivity, D' . Material diffuses from the source through the graphite via both of these paths. The equation that applies to this system is

$$C(x,t)/C(0,t) = 1/2 \exp \left\{ -x \left[\frac{2}{W} \sqrt{\pi D t} \left(\frac{D'}{D} \right) \right]^{1/2} \right\} + 1/2 \exp (-x^2/4Dt) \quad , \quad (1)$$

where $C(x,t)$ is the concentration of the fission product at distance x (cm) and at time t (sec). For the purpose of illustration, the following values of the parameters were used:

$$\begin{aligned} D &= 10^{-7} \text{ cm}^2/\text{sec.} \\ D' &= 10^{-5} \text{ cm}^2/\text{sec.} \\ W &= 10^{-3} \text{ cm.} \\ t &= 10^{-3}, 10^{-2}, 10^{-1}, \text{ and } 10^0 \text{ years.} \end{aligned}$$

Calculations were made every 0.2 cm in the range of $0 \leq x \leq 2$ cm. The results of the calculations, in the form of curves, are shown in Fig. 4.1. The curves show how concentration profiles for a metallic fission product might develop in fuel element graphite assuming simultaneous bulk and pore diffusion.

Analog Method for Calculating Fission-product Release in an HTGR

Several methods (such as FREVAP and FIPER) are currently in use for the computation of fission-product releases from HTGR fuels. These methods range from the use of simple models involving manual calculations to sophisticated models requiring a large computer. A very simple method that utilizes an approximate transient DC analog of a fission-product source that is separated from a sink by n barriers may be a useful tool for understanding fission-product release or as a semiquantitative tool for studying fractional release as a function of time. The method has the virtues of low cost, simplicity, and rapidity.

Fig. 4.1--Calculated concentration profile of a metallic fission product in graphite as a function of distance for different times assuming two diffusion paths

VAPORIZATION OF METALLIC FISSION PRODUCTS FROM GRAPHITE

Studies of the Strontium-Graphite System Using the Knudsen-cell Mass-spectrometric Method

Studies of the vaporization of strontium from TS-688 graphite using the Knudsen-cell mass-spectrometric method were continued. Further experiments employing (1) a tantalum Knudsen cell with a tungsten liner and (2) an all-tungsten Knudsen cell were carried out.

In one experiment an all-tungsten Knudsen cell was used and a thermocouple supplemented the usual optical temperature measurement. The results of this experiment yielded a Freundlich isotherm expression for the pressure versus concentration at 1500°K that is in good agreement with earlier results. However, the heat of vaporization for this experiment does not have as strong a dependence on the concentration as that derived from earlier experiments.

The following equation

$$\ln P = 11.36 - \frac{44.12 \times 10^3}{T} + -0.745 + \frac{4.13 \times 10^3}{T} \ln c, \quad (2)$$

where P is in atmospheres and c is in $\mu\text{mole Sr/gC}$, appears to best represent the results of these experiments combined with the results of earlier experiments. At 1500°K, it is estimated that the pressure will be within a factor of 5 of that given by Eq. (2) 84% of the time, whereas at 1273°K it will be within a factor of 10 of that given by Eq. (2) 84% of the time.

Studies of the Strontium-Graphite System Using the Pseudoisopiestic Method

Studies of the strontium-graphite (TS-688) system using the pseudoisopiestic method (Ref. 7) have continued. Recent sorption runs at 1100°C, using graphite outgassed in situ at 1200°C prior to the start of the run, have yielded anomalous data. The observed loadings were about a factor of two to three higher than those measured at 1000°C at the same strontium partial pressure. This same behavior was noted in sorption runs at 1000°C in which the graphite was outgassed in situ at 1150°C instead of the normal outgassing temperature of 1050°C.

An examination of the conditions of these recent runs revealed that strontium is being desorbed from the reusable tantalum liner in the system at the higher temperatures. At the higher temperature, the Sr pressure above the tantalum may be greater than that of the strontium metal source. This fact was verified by a control run in which no Sr metal was placed in the system, yet after five days at 1100°C the graphite showed a concentration of 0.10 mg Sr/g C.

Finding a suitable container material has posed a major problem in this work. Because of this problem, the work has been discontinued.

It is possible that the container problem could be overcome by use of a disposable liner (to prevent contamination of the specimen by volatile

components in the steel system) or a reusable liner that is unreactive toward strontium (possibly tungsten).

Effect of Irradiation on Cesium Sorption on Graphite

A study on the effect of irradiation on the cesium-sorption characteristics of graphite has continued. The results of the investigation of the sorption characteristics of TS-688 graphite irradiated in the G12 capsule were given in an earlier quarterly report (GA-8662).

One aspect of this study required confirmation. Specimens irradiated at 610° and 1005°C showed a reduction in cesium sorption capacity as a result of pulverizing the graphite to particles smaller than 150 μm prior to the sorption run. A second sorption run conducted during the quarter verified this observation. Thus, it appears that a large fraction (up to about 35%) of the additional sorption sites produced by irradiation can be removed by the mechanical action of pulverizing the graphite. This is an unusual phenomenon inviting further clarification.

Additional cesium sorption measurements are being conducted on grade 9567 graphite, also irradiated in the G12 capsule. The change to a different type of graphite was necessary because no more TS-688 specimens were available. The 9567 specimens were irradiated to doses of 2 to 6 $\times 10^{21}$ nvt over a temperature range of 610° to 1105°C.

The sorption runs have shown a behavior similar to that observed with TS-688 graphite. The sorption capacity of a specimen irradiated at 1005°C to 6.0 $\times 10^{21}$ nvt increased by a factor of 14 over the unirradiated material. This is compared to a factor of 12.2 increase observed with TS-688 graphite from the same cell.

Sorption data obtained for the 9567 graphite have shown a considerable amount of scatter. This scatter was not observed with TS-688 graphite. This behavior, also evidenced in the unirradiated control specimens, suggests that the 9567 graphite is not uniform with respect to sorption characteristics.

FISSION-PRODUCT DIFFUSION STUDIES

Diffusion of Metallic Fission Products in Graphite

Studies of strontium permeation through a graphite have continued. A detailed description of the apparatus and experimental procedure has been presented previously (GA-8662). Experiments carried out at source concentrations of 0.5 mg Sr/g C and 30.6 mg Sr/g C have yielded effective diffusion coefficients of strontium in graphite. The experimental results are given in Table 4.3. The diffusion coefficients were calculated from the steady-state equation for diffusion in a cylinder:

$$\frac{Q_t}{t} = \frac{2\pi D(C_2 - C_1)}{\ln(b/a)} \quad , \quad (3)$$

Table 4.3

EFFECTIVE DIFFUSION COEFFICIENTS FOR STRONTIUM IN GRAPHITE

Exp. No.	(C ₂) Graphite Conc. ^a (mg Sr/g C)	(C ₁) Sr Conc. at Outside Boundary (mg Sr/g C)	(C ₀) Source Conc. (mg Sr/g C)	Time of Run (hr)	Temp (°K)	Diffusion Coefficient (cm ² /sec)	Distribution Coefficient C ₀ /C ₂
1	0.35	0.23	30.6	520	1248	4.7x10 ⁻⁷	87
2	0.3	0.15	30.6	1831	1248	4.3x10 ⁻⁷	102
3	0.007	3x10 ⁻⁶	0.5	418	1273	2.4x10 ⁻⁹	73

^a
Concentration of strontium in graphite at source boundary.

where Q = the total amount of strontium diffused through a cylinder of unit length,

t = time,

b = outer radius of cylinder,

a = inner radius of cylinder,

C_2 = concentration at source boundary,

C_1 = concentration at outside boundary, and

C_0 = source concentration

These values must be regarded as preliminary, however, because problems were encountered in finding a good collector for strontium evaporating from the samples. An inefficient collector could result in measured diffusion coefficients that are unrealistically low. The diffusion coefficient of strontium in this material shows a marked concentration dependence, as can be seen in Table 4.3. A change of less than two orders of magnitude in source concentration results in a change of the diffusion coefficient by a factor of more than 100.

Distribution coefficients are calculated from strontium concentrations in the matrix material and the graphite at the source boundary. The coefficients are of the order of 100 as derived from the data given in Table 4.3.

Because of the difficulties encountered with the collector in the strontium permeation experiments described above, a new experimental apparatus was constructed. In the new apparatus, the graphite crucible containing the strontium source is heated in a tungsten-filament furnace in vacuo, and a water-cooled copper enclosure is used to trap the released strontium. A schematic of the apparatus is shown in Fig. 4.2. Preliminary studies show that the axial temperature variation in the graphite crucible is about $\pm 4^\circ\text{C}$ over a distance of 3 cm at 1000°C . Experiments have been initiated utilizing this apparatus.

In-pile Studies of Strontium Diffusion in Graphite

Six graphite crucibles have been prepared for performing in-pile strontium diffusion measurements. It is planned that the crucibles will be inserted into piggyback locations of Peach Bottom test elements. The cavity of the crucibles will be loaded with Sr^{84} (75% enriched) which yields 65-day Sr^{85} under neutron bombardment. The total release of Sr^{85} will be determined, and the samples will then be sectioned to determine diffusion profiles.

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Fig. 4.2--Strontium permeation apparatus (tungsten-wire furnace)

Task V
RECYCLE FUEL STUDIES

INTERIM STORAGE OF SPENT HTGR FUEL ELEMENTS

Preliminary economic projections (Ref. 1) on the timing of the startup of the first central reprocessing plant for HTGR fuels indicate the lowest long-range integrated cost is incurred when the plant comes on line about seven years after the first large HTGR begins power output. Interim storage facilities must be provided to hold the spent fuel discharged before the reprocessing plant is started up. A study has been undertaken to evaluate the costs associated with these interim storage facilities. In comparison with the interim storage study reported in an earlier quarterly report (GA-8860), this current study is more detailed and broader in scope.

Two storage methods, in air and under water, are included in the current study. Air storage uses a heavy concrete structure for shielding and air for cooling, but underwater storage uses water for both shielding and cooling. Each storage unit is sized to hold the cores from three 1000-Mw(e) HTGRs. This size was selected after taking into consideration the uncertainties in discharge schedules, the uncertainty in the timing of the reprocessing plant startup, and the physical size of the storage unit. Based on the assumed HTGR economy growth and the reprocessing plant timing of Ref. 1, three storage units would be needed. The required number of units may be built on one or more sites. Multiple units on a single site permit common usage of ancillary facilities and better usage of manpower, thus reducing both capital and operating costs. Building the reprocessing plant at the storage site would permit extended use of the storage and ancillary facilities and reduce the reprocessing plant costs accordingly. The storage capacity of one interim storage unit is about twice that needed for surge storage at an operating reprocessing plant.

The spent fuel is packaged at the reactors in containers 17 in. in diameter by 11 ft high each holding four elements and is shipped in large casks mounted on railroad cars. A weld closure is used on the containers. The fuel receiving station at the fuel storage site uses in-air unloading with the fuel shipping cask remaining in place on its railroad car for the underwater storage case as well as the air storage. With air storage, each storage vault requires a receiving station, but one receiving station can service all of the underwater storage basins on the same site. This results from the relative ease of transporting radioactive material through a water shield. The receiving station for the underwater storage case is more complex than for air storage in that it includes leak checking and repackaging stations for the fuel containers. This is considered necessary because with underwater storage the consequences of a stored, leaky container would be much more serious.

The initial storage unit built on a site must include all necessary ancillary facilities, such as a cask decontamination area, power supply building ventilation exhaust air treatment, liquid waste treatment, change room, offices, and an analytical laboratory. Thus, the capital cost of the initial installation is substantially more than that of additional storage units erected on the same site. Table 5.1 shows the estimated costs associated with interim storage (three 1000-Mw(e) HTGR cores per unit) for the two storage-method studies.

Table 5.1
ESTIMATED COSTS ASSOCIATED WITH INTERIM STORAGE

	<u>In-Air Storage</u>	<u>Underwater Storage</u>
Capital cost, \$10 ⁶ :		
First unit	4.9	7.0
Additional units	2.1	2.8
Operating cost, \$10 ⁶ /yr:		
One unit	0.48	0.53
More than one unit	0.58	0.53

Fuel element containers are not included in the above costs. The cost of the containers for each storage unit is \$0.9 x 10⁶.

If an HTGR fuel reprocessing plant similar to that described in Gulf General Atomic study (Case III A in Ref. 2) is later built on the same site, its capital cost could be reduced as indicated in Table 5.2.

Table 5.2
CAPITAL COST REDUCTION

<u>Reprocessing Plant</u>	<u>Capital Cost (\$10⁶)</u>
Case III A	30.8
Combined with in-air storage vault	25.7
Combined with water storage basin	23.6

The lower cost for the combination with the underwater storage unit results from the excess basin capacity eventually being used for the interim storage of calcined high-level waste. Note that the cost of the reprocessing plant and the first storage unit (see Table 5.1) is less than the Case III A plant cost. This is due to the fact that the fuel storage vault in Case III A is designed to receive either canned or uncanned fuel. Case III A costs include captive fuel storage containers, whereas the interim storage units excluded fuel container costs.

Assuming the HTGR economy growth and reprocessing plant start as contained in Ref. 1, and the construction of the three storage units and reprocessing plant on the same site, the interim storage costs are \$370 and \$400 per fuel element for in-air and underwater storage, respectively.

COMBINED HTGR REPROCESSING-REFABRICATION PLANT STUDY

Work was initiated on a study of a complex to both reprocess and refabricate HTGR fuels. The study is based on completed central plant studies (Refs. 1 and 3) and the fuel element design, plant throughput, and basic process flowsheets for the combined plant are the same as those used in these earlier studies. Block flow diagrams and material balances are in preparation.

NATIONAL HTGR RECYCLE DEVELOPMENT PROGRAM

The National HTGR Recycle Development Program has been revised in part by Oak Ridge National Laboratory and Gulf General Atomic to incorporate all review comments and to reflect the effects of the new planned commercial recycle plant startup date of 1982.

HEAD-END REPROCESSING

Effort on head-end reprocessing during the quarter has been directed toward fuel element debonding by crushing and by broaching and splitting.

Block Crushing

A flowsheet has been prepared for a series of experiments to determine the extent of particle damage and the size range of broken-down fuel rods after small samples of fuel elements have been crushed and tumbled. A simplified version of this flowsheet is shown in Fig. 5.1. Experimental work has started with enriched fuel rods that had failed to satisfy the rigid specification for the second Fort St. Vrain proof test element because of a slightly defective SiC coating. Because these rods satisfied all other requirements, any head-end experimental findings can be regarded as applicable to production-quality fuel rods. The initial results are very encouraging, but further work on larger quantities and on larger fuel elements must be done before firm conclusions can be reached.

It should be noted that representative irradiated samples will not be available for some time, and until they are available, development work must proceed with unirradiated fuel elements and rods. However, each result is viewed in the light of how it would have been affected if the material had been irradiated to full term. It is hoped that by this means, all results on unirradiated material will be applicable to the full-scale reprocessing plant.

Jaw Crushing. A bare rod 7/16 in. in diameter and about 11 in. long, a rod encased in a graphite tube, and a graphite tube were crushed separately

Fig. 5.1--Simplified flowsheet of crushing and tumbling concept

in a 3-in. jaw crusher with a 3/8-in. jaw gap and a flywheel speed of 300 rpm. Each item took 10 sec to feed, which gave a processing rate of approximately 1 in./sec of fuel rod. The crushed product was sieved; the results are given in Table 5.3.

Table 5.3
SIEVING RESULTS AFTER USING 3-IN. JAW CRUSHER
(Values in grams, percentages in parentheses)

Item	+5/16 in. mesh	+1/8 in. mesh	+835 μ m	+420 μ m	-420 μ m	Total
Bare rod	40.92 (56.3)	16.59 (22.8)	13.33 (18.4)	0.77 (1.1)	1.09 (1.5)	72.70
Rod in graphite tube	147.07 (60.3)	44.43 (18.2)	47.32 (19.4)	1.40 (0.6)	2.99 (1.3)	243.21
Graphite tube only	135.83 (88.5)	11.96 (7.8)	3.75 (2.4)	0.50 (0.3)	1.26 (0.8)	153.30

The data show that the bare and encased rods crushed in a similar manner but that the graphite tube was broken down to a higher fraction of larger pieces.

The graphite and particles were separated (using tweezers) in the +5/16- and +1/8-in. size range of the encased rod product and weighed. The results, given in Table 5.4, show that (1) the graphite fraction distribution is very similar to that of the graphite tube only (Table 5.3) and (2) the encased rod shattered into a larger fraction of smaller particles than the bare rod.

Table 5.4
GRAPHITE AND PARTICLE CONTENTS OF EACH FRACTION

Fraction (in.)	Graphite (%)	Particles (%)
+5/16	84.0	7.8
+1/8	12.5	31.8
-1/8	3.5	60.4

Photomicrographs showed that the -420 μ m fraction contained many whole particles in addition to graphite-matrix fines. The -420 μ m fraction was therefore sieved through a 300- μ m screen, and the 1.38 g of -300 μ m fines were microscopically examined for broken particles. There were none; only graphite/matrix fines plus some pieces of coating shells could be seen.

It was concluded from these initial trials that:

1. Crushing and sieving is a promising method of largely separating graphite from particles.
2. The particle agglomerates are small compared with the jaw gap when the rod is initially encased in graphite.
3. The proportion of small agglomerates is high, which is a promising feature if this material is to be fed to a fluidized bed.
4. The act of crushing in a jaw crusher does not damage many particles.

A second fuel rod encased in graphite has been crushed in the 3-in. jaw crusher at 300 rpm and jaw gap of 1/2 in. A thorough examination of the product will be made.

Tumbling. The objective of tumbling is to break down particle agglomerates to $-1/8$ in. so that a narrow size range may be fed to the burner (see Fig. 5.1). In the crushing results listed in Table 5.4, the fraction of particles in the $-5/16$ to $+1/8$ in. range is too large to feed to the graphite burner, and the agglomerate size is too large to be fed, for example, to a fluidized bed. Under these circumstances, it is necessary to be able to break down agglomerates to $-1/8$ in. mesh and at the same time to gain the advantage of a further separation of particles from graphite.

The scheme of operation offers the following advantages: (1) A large amount of graphite (194 kg/hr) can be accommodated in a burner that is designed to handle a large heat release rate ($\sim 6 \times 10^6$ Btu/hr) and a large off-gas volume of moderately low activity, and the temperature need not be very carefully controlled; (2) A small amount of carbon (30 kg/hr) can be burned from particles in another burner that is designed to handle a much lower heat-release rate ($\sim 10^6$ Btu/hr), a much smaller off-gas volume at higher activity, and the temperature and other conditions can be carefully controlled.

The tumbler used in some trials was 9 in. long and 4-1/2 in. in diameter, and was fabricated of polyethylene. It was rotated at 24 rpm around an axis perpendicular to the length. The method of operation was (1) to charge the drum with a weighed quantity of $+1/8$ in. mesh agglomerates, (2) to rotate for a given period, (3) to sieve the contents through a $1/8$ in. screen and return the oversize to the drum, (4) to weigh the product, and (5) to repeat operations (2) to (4). When most of the feed had appeared as product, the experiment was terminated.

The results of feeding crushed bare rod product to the tumbler are given in Fig. 5.2 and in Table 5.5. The results given in Fig. 5.2 show that initial charge weight and size have virtually no effect on the attrition rate. Factors affecting attrition rate will undoubtedly be length of tumbler, presence of pieces of graphite, and the screen size through which the product is sieved. These factors will be investigated as testing material becomes available.

The results in Table 5.5 show that the size distribution in the three products is very uniform. Microscopic examination of the $-300 \mu\text{m}$ fraction showed that the debris was composed of a few broken shells and mainly materials that looked like coke (probably matrix fines); there were no broken particles and no whole particles.

Fig. 5.2--Attrition of agglomerates caused by tumbling in a 9-in.-long drum with no graphite lumps present

Table 5.5
SIEVE FRACTIONS OF TUMBLER PRODUCT

Run No.	Initial Weight (g)	Initial Size (in.)	Final Size Distribution (%)				Tap Density of Product (g/ml)
			+1/8 in.	+835 μm	+300 μm	-300 μm	
3	16.57	-5/16, +1/8	1.0	84.3	14.1	0.7	1.74
4	13.20	+5/16	1.4	80.9	16.4	1.3	1.74
5	26.00	+5/16	1.6	82.7	14.2	1.5	1.74

A 0.1-g sample of the -300- μ m material was fired in air at 900°C. The final weight was 0.0013 g. Microscopic examination showed the ash to be broken pieces of SiC shells. There was no sign of ThO₂ or U₃O₈.

A longer tumbler is being installed to increase the throughput and to determine the effect of length on particle damage.

Burning. The objective of burning is to remove the matrix and outer PyC coat from TRISO-II particles in order to expose the SiC coat. A sieving operation is then used to separate the fissile from the fertile particles.

A few trials of particle burning were carried out in air in a muffle furnace, but the results were inconclusive because the particles were lightly bonded into agglomerates, which made separation of the fertile and fissile particles impossible by screening. This phenomenon is being investigated.

The effect of burning on particle damage was observed by microscopically examining the residual particles. A few trials gave the results shown in Table 5.6. The results show that static burning has only a small effect on particle damage.

Table 5.6

DETERMINATION OF BROKEN PARTICLES BY MICROSCOPIC EXAMINATION

Run No.	Size Range	Initial Weight (g)	Final Weight (g)	Temp (°C)	No. of Broken Particles
3	+835 μ m, -1/8 in.	0.3963	0.2947	900	0
	+300 μ m, -835 μ m	0.3141	0.2203	900	2
5	+835 μ m, -1/8 in.	1.1867	0.8839	650	6

STABILITY OF TRISO-COATED PARTICLES DURING FLUID BED OXIDATION

Studies of the stability of TRISO-coated particles during oxidation in a fluidized bed have been resumed as reported in the previous quarterly report (GA-9090).

The results of early studies using TRISO-I particles showed that approximately 3% to 4% of the particles were broken during the oxidation process. The breakage varied from 0% to about 10%. The early studies, however, were more qualitative than quantitative, since the breakage fraction was estimated visually by microscopic examination after the run. In addition, it is now known that the samples used in these studies initially contained a number of defective particles, which would be reflected in the observation of an unusually high breakage rate during the oxidation. These data are now being summarized.

A quantitative measure of the number of particles broken or cracked during the process is being obtained by leaching the fluid bed sample following

the oxidation, and determining the fraction of initially charged uranium that can be leached from the bed. Some data obtained using this procedure were presented in the previous quarterly report, and additional data are being obtained. In addition, the TRISO-II particles being utilized in current studies have a very low fraction of defective particles in the starting material. The results of current oxidation studies are therefore not being perturbed by the use of samples of lesser quality.

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Task VI
PHYSICS METHODS

TWO-DIMENSIONAL DIFFUSION DEPLETION CODES

Under the HTGR Base Program two types of two-dimensional diffusion-theory codes have been developed:

1. A fast two-dimensional diffusion-theory code for survey studies with certain restrictions in the number of energy groups, scattering matrix, mesh spacing, region structure, etc.
2. A very flexible but somewhat slower multigroup diffusion-theory code with few restrictions, allowing very detailed and accurate calculations.

The combination permits inexpensive survey studies that can be extended and supplemented by very detailed and accurate design calculations. The two-dimensional four-group diffusion code GAUGE (Ref. 1) belongs in the first class of codes and has been described in an earlier quarterly report (GA-8200). This code has since been improved considerably by incorporating a coarse-mesh rebalancing method and a special asymptotic fission source extrapolation; these improvements decreased the necessary computer time by more than 80%. The two-dimensional multigroup diffusion-theory code GAMBLE-5 (Ref. 2) belongs in the second class of codes and has also been considerably improved by incorporating a very flexible coarse-mesh rebalancing scheme described in the quarterly report for the period ending November 30, 1967 (GA-8356).

GAUGE is restricted to four energy groups, allows only downscattering to the next lower energy group, and requires a so-called "patch" structure with a uniform hexagonal mesh. GAMBLE-5 has none of these restrictions, but it is restricted to a rectangular (x,y) or (r,z) mesh. Most HTGR reactors have a basically hexagonal structure, and a flexible multigroup diffusion theory code based on a triangular mesh was therefore highly desirable. GAMTRI (Ref. 3) and BUGTRI, the depletion program based on GAMTRI, fill this need.

The three depletion codes GAUGE, BUG-2 (Ref. 4), and BUGTRI are compared in Table 6.1.

Two-dimensional Multigroup Diffusion-theory Code GAMTRI

GAMTRI is a two-dimensional multigroup diffusion-theory code for a uniform triangular mesh. It uses a line-block-relaxation technique for the numerical solution of the difference equations. For convergence acceleration, an alternating group-column, group-row rebalancing method and an asymptotic flux extrapolation technique are utilized. GAMTRI belongs to the second class of codes mentioned above. The flexibility of GAMTRI with respect to the geometrical configuration needs some further explanation.

Table 6.1
TWO-DIMENSIONAL DEPLETION CODES

	GAUGE	BUG-2	BUGTRI
Diffusion			
Max. no. groups	4	10	10
Max. no. space meshpoints	5,000	20,000	20,000
Geometry	Uniform hexagonal	Nonuniform (x,y) & (r,z)	Uniform hexagonal
Scattering matrix	Downscattering to next lower energy group	Arbitrary	Arbitrary
Boundary conditions	$\phi = 0$	$a\phi + b\frac{\partial\phi}{\partial\eta} = 0$	$a\phi + b\frac{\partial\phi}{\partial\eta} = 0$
Nondiffusion regions	No	Yes	Yes
Numerical method used	Block-inversion technique combined with special "patch" coarse-mesh rebalancing and asymptotic fission source extrapolation method	Line-block-relaxation combined with general coarse-mesh rebalancing method for space and energy structure, asymptotic fuel extrapolation method	Line-block-relaxation technique combined with alternating column-row rebalancing method and asymptotic flux extrapolation method
Restrictions	Configuration must have "patch" structure; each hex block must have homogeneous material properties	Essentially no restrictions	Essentially no restrictions
Depletion			
Max. no. nuclides	40	40	40
Max. no. burnup regions	280	255	255
Max. no. heavy nuclides	25	25	25
Max. no. fission products	25	25	25
Max. no. cross-section blocks	80	100	100
Max. no. cross-section sets	20	30	30
Reload feature	yes	yes	yes
Criticality search	yes	no	no

GAUGE requires a patch structure of the reactor configuration as shown in Fig. 6.1, where each patch has 13 interior meshpoints and consists of seven hexagonal fuel elements or sections of fuel elements. There is no such restriction in GAMTRI. The smallest unit for which different material properties can be specified in GAUGE is a hexagonal block, as shown in Fig. 6.2. For GAMTRI the smallest unit is a triangle. Hence, GAMTRI can treat a thin boronated ring (see Figs. 6.2 and 6.3) accurately, whereas this is not possible with GAUGE. These flexibilities also permit a better representation of the outer reflector in GAMTRI than in GAUGE.

BUG/BUGTRI Depletion Routine and Burnup Code Calculation Framework

The BUG and BUGTRI two-dimensional depletion codes have been developed using a common calculational framework, depletion section, and linkage to the two-dimensional diffusion-theory routines. The input is identical for both codes (except for the region map specification). The depletion routine is essentially the same as that developed for the GAUGE program. Some of the features of these depletion programs are:

1. A flexible depletion scheme defined by user input data.
2. An efficient burnup routine that permits an optimal choice between finite-difference and analytic solutions for the nuclide depletion equations.
3. The ability to apply concentration-dependent self-shielding factors to any nuclide.
4. Flexibility with regard to changes in power level, burnup intervals, partial refueling, edit options, and in saving data on tape for continuation runs.

The burnup calculation for a reactor follows a conventional scheme. For a number of time points, specified by the user, static diffusion calculations are performed, and it is assumed that the total reactor power and the spatial flux distribution remain constant for each time step. The reaction rates for all nuclides in a region are calculated using the average group fluxes for that region.

For reactors with internal conversion ratios not close to unity, the macroscopic fission cross section may change rapidly. The flux amplitude and the spectrum will then vary considerably, even over time periods for which the assumption of a constant spatial flux shape remains valid. Therefore each long time step may be subdivided into a number of small time steps of equal length, so that the flux amplitude can be readjusted more frequently to maintain the power level constant. The flux-amplitude normalizing factor is calculated using all group fluxes, but only the thermal flux is renormalized. This procedure leads to a reasonable approximation for thermal reactors.

The use of small burnup time steps is also indicated if nuclides with concentration-dependent self-shielding factors appear in the problem. The group-dependent self-shielding factors g_g are defined by the expression

$$g_g = \frac{c}{1 + \kappa \Sigma_a r + \lambda (\Sigma_a r)^2} \text{ or } g_g = K_1 + K_2 N + K_3 N^2 + \dots K_6 N^5$$

$$\Sigma_a = N \sigma_{a,g} ,$$

where c , κ , λ are fitting constants, and r is the radius of the lump, $\sigma_{a,g}$ is the unshielded microscopic cross section of absorber, and N is the atom density of absorber.

At the end of each long burnup step, a partial refueling may occur. But, whether or not a reload has been requested, a diffusion theory calculation will be performed at the end of this time step. If a reload follows, a diffusion calculation will also be done for the conditions of the reactor immediately after the reload.

The general depletion scheme used in these codes allows the user to formulate within certain limits the depletion equations to be solved by the program. This is done by specifying for each computer run the coupling of all nuclides with their parent or daughter products.

In using such a system, a basic difficulty is always encountered. In most cases there will be some nuclides (e.g., Pa^{233} , Np^{239} , Xe^{135}) with large reaction rates. If fixed depletion chains are built into the code, one can explicitly use analytical solution for all nuclides in the chain. On the other hand, since the reaction rates are assumed constant for each time step, one would still have to use relatively small time steps Δt . For long burnup chains of five or more coupled nuclides, this would not only lead to considerably longer computing times than for a difference approximation but, might also result in a considerable loss of significant figures because of the nature of the solution. Hence, if the existence of fast-saturating nuclides would not prohibit its use, it would be preferable to use a difference approximation that is faster and more accurate for small time steps. In GAUGE, BUG, and BUGTRI, both methods for handling fission products are used. For nuclides with small reaction rates, it would be preferable to use a difference approximation to avoid the buildup of round-off errors for long chains, while at the same time using an analytical treatment for the rest of the nuclides. However, given the reaction rates, the decision to use a particular approximation for each nuclide is dependent on the length of the time step Δt . With increasing Δt , more and more nuclides would have to be treated analytically. In this way the maximum length of the time step is no longer limited by the truncation error of the difference solution, while for small time steps it is still possible to take advantage of the speed of the difference approximation.

Based on an input error criterion, ϵ_B , the code automatically decides for each time step and for every nuclide what approximation will be used. The error ϵ_B is a restriction for the truncation error allowed per time step. By solving the equation

$$e^{-a} - \frac{2 - a}{2 + a} = \epsilon_B ,$$

Fig. 6.1--Basic parallelogram configuration for HTGR calculational models

Fig. 6.2--Sector of HTGR coarse mesh

—— GAUGE OUTER REFLECTOR SURFACE
..... GAMTRI OUTER REFLECTOR SURFACE

Fig. 6.3--Sector of HTGR fine mesh

a critical value $a = a_\epsilon$ is determined for the magnitude of the product

$$|d_{ii}|\Delta t \quad ,$$

where d_{ii} is the removal term in the burnup equation. Therefore, if

$$|d_{ii}| > \frac{a_\epsilon}{\Delta t} \quad ,$$

an analytical solution is requested for nuclide i .

The nuclear burnup equations to be solved are assumed to have the following form ($d_{ii} < 0$):

$$\frac{dx_i}{dt} = + d_{ii}x_i + \sum_{j=1}^{i-1} d_{ij}x_j \quad , \quad i = 1, \dots, N_x \quad ,$$

or in compact matrix notation,

$$\frac{dx}{dt} = Dx \quad ,$$

where x is a vector whose components comprise the nuclear densities of all burnable nuclides in the problem. The coefficients d_{ij} are the elements of the lower triangular matrix D and represent the reaction rates for the transmutation of nuclide j into nuclide i . The diagonal elements d_{ii} correspond to the total removal rate of nuclide i from the system; hence

$$|d_{ii}| > \sum_{j=i+1}^{N_x} d_{ji} \quad .$$

The matrix D is assumed to be a constant lower triangular matrix. Since the reaction rates d_{ij} depend both on the time t and on the densities x , the depletion matrix D must be recalculated for each time step Δt . The fact that the variation of the reaction rates is replaced by a step function limits the time step length. With the assumption of a triangular form of the matrix D , feedback reactions are excluded from the depletion chain. From a practical point of view, this is not very restrictive, since feedback reactions are relatively unimportant.

Other than the triangular form no additional limitation for the structure of the matrix D is required. The coupling of the nuclides, which determines the depletion chain, is defined by the user at execution time. The code permits specification for each nuclide, two capture parents, one n-2n parent, and a decay parent. In addition to the radioactive decay constant a leakage

constant is defined to account for possible leakage of volatile nuclides. The resulting nuclide scheme must fulfill the requirement that there are not more than two capture daughters for each nuclide. Fission yields for the formation of each individual fission product may be specified for all heavy nuclides.

Special consideration is given to the linkage between fuels and fission products. Since no direct feedback between fission products and fuels exist, one can solve the burnup equations for all fuel chains first, to obtain the fuel concentrations $\bar{x}_i$, $i = 1, \dots, N_{\text{fuel}}$, averaged over the time step Δt . For each fission product, $j = N_{\text{fuel}} + 1, \dots, N_x$, a constant average production rate $\bar{S}_j$ is then calculated

$$\begin{aligned}\bar{S}_j &= \sum_{i=1}^{N_{\text{fuel}}} d_{ij} \bar{x}_i \\ &= \sum_{i=1}^{N_{\text{fuel}}} \Gamma_{ij} \bar{x}_i \sum_{g=1}^G \sigma_{fi}^g \phi_g ,\end{aligned}$$

to replace the actual time-dependent source $S_j(t)$ for the time step Δt . Γ_{ij} is the yield of fission product j from fissions of nuclide i . This approximation is consistent with the assumption that the partial thermal power,

$$P = \sum_{i=1}^{N_{\text{fuel}}} x_i(t) \sum_{g=1}^G \sigma_{fi}^g \phi_g ,$$

generated in a region is constant during the time step Δt . If the latter assumption is fulfilled, then the error in replacing $S_j(t)$ by a constant source rate S_j can usually also be ignored.

Comparison of GAUGE and BUGTRI for an HTGR Depletion Problem

The basis for the comparison of GAUGE and BUGTRI is an unrodded and axially homogenized HTGR core. The BUGTRI model duplicated the GAUGE model (that is, four groups, coarse mesh, loading specification by hexagonal block, and the noncircular GAUGE-type outer boundary). Loadings, cross-section assignments, and axial leakages were specified identically in the two calculations.

The patch power distribution and k_{eff} for three time points are predicted by GAUGE and BUGTRI are shown in Fig. 6.4. There is a difference of about $2 \times 10^{-4} \Delta k$ that exists between the two analyses, and the BUGTRI consistently shows slightly higher powers in some of the outer ring patches. The differences are both attributable to the outer boundary conditions. Table 6.2 shows

0 DAYS

10 DAYS

50 DAYS

Fig. 6.4--Whole core depletion (patch power distribution)

the initial timepoint neutronics as calculated by both GAUGE and GAMTRI. Both the flux and absorption spectra are very similar; perhaps the GAMTRI analysis indicates a slightly softer flux spectrum. However, the radial leakage values show higher fast and total leakages in GAMTRI that are typical of the differences observed previously because of the outer boundary condition.

Table 6.2
INITIAL CORE NEUTRONIC COMPARISON

Group	ϕ	Absorptions	Radial Leakage (%)
GAUGE ($k_{\infty} = 1.099247$)			
1	0.1894	0.0072	5.288
2	0.4465	0.3095	1.223
3	0.0668	0.0427	0.141
4	<u>0.2974</u>	<u>0.6407</u>	<u>-3.746</u>
Total	1.0	1.0	2.906
GAMTRI ($k_{\infty} = 1.10124$)			
1	0.1887	0.0071	6.064
2	0.4457	0.3080	1.043
3	0.0667	0.0423	0.118
4	<u>0.2989</u>	<u>0.6426</u>	<u>-4.159</u>
Total	1.0	1.0	3.066

THREE-DIMENSIONAL DEPLETION CODES

The development and use of three-dimensional depletion codes based on three-dimensional difference methods is on the borderline of feasibility for present-day computers in case of large power reactor problems. Therefore, attention has been focused on an efficient steady-state three-dimensional code, and several simplifying assumptions have been made to reduce the complexity of the problem to a manageable degree without imposing too severe restrictions for HTGR reactor calculations. The result of this development is the three-dimensional few-group neutron-diffusion-theory code GATT (Ref. 5). This code can be coupled with standard depletion-theory routines to provide a three-dimensional depletion code. For small HTGR power reactors this appears feasible for the 1108 computer.

GATT is a three-dimensional few-group neutron-diffusion-theory program for calculating the detailed flux and power distribution for reactors with an hexagonal core configuration. The code utilizes a uniform triangular mesh in the horizontal mesh planes and assumes a relatively simple region structure in the axial direction. The code allows a maximum of four energy groups, with down-scattering from one group into the next, and up to about 100,000 mesh points. GATT, like its two-dimensional counterpart GAUGE, makes use of

a direct block-inversion technique. This method leads to an improved convergence of the inner spatial flux iterations. The outer fission source iteration, on the other hand, is accelerated by an asymptotic source extrapolation that is applied periodically to damp out the next higher eigenmodes. The limitations of the code are:

1. Number of energy groups 4
2. Number of patch rings, not
counting the central patch 4
3. Number of z-planes 81
4. Total number of spatial mesh points 104,487
5. Number of macroscopic cross section
sets (mixtures) 40
6. All fission neutrons are born in energy group 1.

A four-group problem for a 300-Mw(e) reactor with partially inserted control rods and approximately 45,000 mesh points took 120 min to converge assuming a very tight convergence criterion (10^{-5}).

The GATT code is being finally debugged and evaluated. The documentation has been completed and the code will be sent to the Argonne Code Center.

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Task VII
EXPERIMENTAL PHYSICS

LARGE CONTROL ROD EXPERIMENTS IN LATTICE ASSEMBLY NO. 2

Lattice assembly No. 2, which is characterized by a C/Th ratio of 200 and a C/U²³⁵ ratio of 2500, was described in earlier quarterly reports. The core configurations with one or two large control rods located within the lattice assembly were given in the previous report. A brief summary to those configurations is as follows:

- Core A - The standard core assembly for lattice assembly No. 2 with no large poison rod; the outer radius is 80.76 cm.
- Core B - Lattice assembly No. 2 with one large poison rod in the center of the lattice; the outer radius is 88.57 cm.
- Core D - Lattice assembly No. 2 with two adjacent large poison rods, one of which is in the center; the outer radius is 90.80 cm.
- Core E - Lattice assembly No. 2 with two large poison rods separated by the central HTGR lattice fuel element; the outer radius is 92.49 cm.

ANALYTICAL RESULTS FOR LARGE CONTROL RODS

The reactivity worth for the single large control rod was calculated in two ways with very little difference in results. Using ten broad groups, the worth was calculated explicitly using the one-dimensional radial transport theory model. The worth was also calculated using the lattice region as a large supercell, calculating the self-shielding factors for the boron in the rod and then homogenizing the boron into the lattice region in a full core radial diffusion theory calculation. The results are shown in Table 7.1. In addition, the calculated worth of a single large rod for different sized cores is also given. This difference is due entirely to the change in leakage with the large control rod at the center of the core. The measured excess reactivity was essentially the same for Cores A and B. The difference in calculated excess reactivities was only about 0.005 Δk . Since the total rod worth is about 0.07 Δk , it can be concluded that the computed worth of these rods was about 7% too high.

For the pair of large control rods, the analysis was made using ten broad groups and two-dimensional transport theory. However, the analysis is complicated by the necessity of representing a large cylindrical poison material in XY geometry. In addition, if an explicit full core calculation is made, the nearly radial outer boundaries of the lattice, C/U-2500 core, and the reflector regions must also be represented in the XY calculational geometry. But there is a computer restriction on the maximum allowable number of mesh

points, and if the P_1S_4 approximation is used in the transport-theory calculation, an array of only 40 x 30 mesh lines is permissible.

Table 7.1

CALCULATED AND MEASURED REACTIVITY WORTH FOR SINGLE LARGE CONTROL ROD AT CENTER OF LATTICE CORE ASSEMBLY

Core Description	Calculated Values			Measured k_{eff}
	Δk	$\Delta \rho (\%)$	k_{eff}	
<u>Core A:</u>				
Reference core with no large rods	---	---	1.024	1.014
<u>Core B:</u>				
Fuel and reflector added but no rods	0.067	8.80	1.091	---
With one large rod:				
Explicit transport theory calculations	0.070	9.20	1.021	1.014
Diffusion-theory model with boron added with self-shielding factors	0.073	9.60	1.018	---
One large rod in core A	---	12.10	---	---
One large rod in core B	---	9.20	---	---
One large rod in core E	---	8.30	---	---

Even with quarter-core symmetry, as was the case in core E, an inadequate XY description for the control poison was obtained when a full core transport theory calculation was attempted. Too many mesh lines were required to describe the outer boundaries of the core and reflector regions. This inadequacy was evidenced by negative fluxes internal to the control rod and integrated flux values for the various core and reflector regions that did not agree with those measured (see Table 7.4).

A subsequent calculation was made in which the lattice region was treated as a large supercell. This calculation was done using two-dimensional transport theory and the mesh description shown in Figs. 7.1 and 7.2. The criteria used to describe the control rod and sleeve were (1) the correct volumes for both the carbon sleeve and the boron carbide region were maintained, (2) the outer mesh points for the poison region fell on the cylindrical surface of the control rod, and (3) on the surface of the poison region at least one internal mesh point was located for each outer boundary. The correct total volume for the lattice region was also maintained. Despite the detailed mesh description for the control rod shown in Fig. 7.2, some negative fluxes were still obtained within that region but their magnitude was sufficiently small that the results were not affected.

Fig. 7.1--XY Description of lattice supercell (quarter-core symmetry)

Fig. 7.2--XY Description of cylindrical annular control rod and carbon sleeve

Some results of the supercell calculations are summarized in Table 7.2, where the flux self-shielding factors calculated for the boron in the rod are given for several cases of interest.

Table 7.2
CALCULATED BROAD-GROUP SELF-SHIELDING FACTORS FOR BORON IN LARGE
CONTROL RODS CONTAINED IN LATTICE REGION

Broad Group Description, Upper Energy (ev)	Core B One Large Rod Radial 1-D $\rho_B = 1.03$ (g/cm ³)	One Large Rod ^a Radial 1-D $\rho_B = 0.51$ (g/cm ³)	Two Large Rods Separated By Fuel Element, Core E; 2-D Supercell	Two Large Rods Adjacent Core D; 2-D Supercell
14.9 + 6	0.7042	0.7541	0.7052	0.6534
1.35 + 6	0.8143	0.8451	0.8149	0.7700
1.83 + 5	0.6007	0.7491	0.6124	0.5547
961.1	0.3042	0.4867	0.3142	0.2711
214.4	0.1492	0.2863	0.1559	0.1282
47.85	0.0445	0.0937	0.0470	0.0372
2.38	0.0135	0.0283	0.0143	0.0112
0.414	0.00574	0.0119	0.00605	0.00473
0.10	0.00333	0.00689	0.00345	0.00270
0.04	0.00191	0.00396	0.00188	0.00148

^aThe large control rod measurements were all made with a full-density loading of B₄C particles, which gives a boron density of 1.03 g/cm³, but this case was calculated for comparison with lower boron density cases.

It is of particular interest to note that there is essentially no difference in the values of the self-shielding factors calculated for a single large rod or the pair of large rods which are separated by a fuel element. (This is consistent with the results of other HTGR control rod analyses.)

In the calculation, a zero flux gradient was assumed at the outer boundary of the lattice region. Measurements reported in the previous quarterly report (GA-9090) show this to be within about 10% of being correct for those cases where a pair of large rods are inserted. As a result, the average flux for the supercell is probably a little low for those cases, giving self-shielding factors that are too low. This is consistent with the total calculated reactivity worth for a pair of large control rods being higher than measured.

The total calculated reactivity worth for a pair of large control rods was obtained by homogenizing the boron from the control rods into the lattice region using the appropriate self-shielding factors and performing a full core

radial diffusion-theory calculation. The results of this analysis are given in Table 7.3. As shown there, the calculated difference between the two configurations with a pair of large control rods present agrees well with the measured difference. However, the effective multiplication factor for those two core assemblies is essentially the same as that measured, whereas for the no-rod configuration this is not true. There the calculated value is too high by about 0.01 Δk , or \$1.50 in reactivity. This implies that the total calculated worth for the pair of large control rods is about 7% too high. This is consistent with the results mentioned earlier.

Table 7.3

CALCULATED AND MEASURED WORTH OF PAIR OF LARGE CONTROL RODS

Core Description	Calculated Values		Measured Values	
	(\$)	(k_{eff})	(\$)	(k_{eff})
<u>Core A:</u>				
Reference core with no large rods	---	1.024	---	1.014
<u>Core D:</u>				
With two large rods adjacent	12.90	1.014	---	1.012
<u>Core E:</u>				
With two large rods separated	14.90	1.011	---	1.014
Calculated difference between core D and core E	2.00	---	2.31	---

Integrated flux measurements were made for these cores with thin copper foil located at the midplane of the split-bed core assembly. It was located so that the integrated copper activity for radial regions of the core was known. The comparison of these measurements with those calculated is given in Table 7.4; good agreement was generally obtained with the measured data. The major disagreement was for the reflector region, where it was difficult to maintain a uniform reflector thickness in the actual core assembly.

DISTRIBUTED POISON EXPERIMENTS

To assist in the comparison between the calculated and measured reactivity worths for the large control rods, distributed poison measurements were made. In these experiments boron (in the form of glass tubes) was added to the void holes within the HTGR lattice fuel elements until the distributed poison equivalent of the large control rod or control rod pair was obtained. This was done for two configurations: one large control rod in the central location (core B), and two large control rods separated by the central HTGR lattice fuel element (core E).

Table 7.4

COMPARISON OF CALCULATED TO MEASURED COPPER ACTIVATION AT CORE MIDPLANE
FOR RADIAL REGIONS OF VARIOUS CONTROL ROD CONFIGURATIONS

Core Region	Ratio ^a = $\frac{\text{Measured Integrated Copper Activity}}{\text{Calculated Integrated Copper Activity}}$				
	Core A No Large Rod Reference	Core D Two Large Rods Adjacent	Core E Two Large Rods, Full Core 2-D Calculation	Core E Two Large Rods Separated, Boron Homogenized	Core E Distributed Poison, Two Rods Equivalent
HTGR lattice	0.99	0.98	1.05	0.97 0.99	0.99
C/U 2500:					
Adjacent to lattice	0.98	1.00	1.01	0.99	0.99
Middle	0.99	1.02	0.97	1.00	1.00
Outer	1.02	1.01	1.03	1.03	1.02
Reflector	1.07	1.03	0.96	1.10	1.04

^a
Normalized so that the total measured and calculated activity in the core and reflector are equal.

Natural boron in the form of B_2O_3 contained in silica-glass tubing was used as the distributed poison. The silica glass contained 2.9 wt-% B_2O_3 and had an outer diameter of 0.9 cm with a wall thickness of 0.1 cm. The remainder of the material in the glass was primarily SiO_2 . It was located within the void holes of the lattice fuel elements as shown for one of the loadings in Fig. 7.3. Glass tubing was used in these experiments because the boron is distributed homogeneously with no detectable particle effects. The boron content is well controlled in the manufacturing process and each 3-ft-long glass tube contains 0.4433 g of natural boron.

A summary of the distributed poison added for the two configurations is given in Table 7.5. For both cases in which the distributed poison was added, it was only located within the lattice assembly.

Some difficulty in measuring the reactivity worth of the distributed poison was encountered due to interaction effects between the glass tubes. The reactivity worth of the glass tubes as a function of radial position was measured by removing selected tubes at each of about eight radii. The integral worth of all the tubes was then obtained by adding the worth of all the tubes at each radius. The results are shown in Table 7.6. For both cases, the integrated worth turned out to be too low, as might be expected if interaction is present.

In measuring the reactivity worth of the glass tube at a location, the glass tube was removed causing the flux to peak there. This increased the worth of the adjacent poison tubes and reduced the effective measured worth of the poison removed. An attempt was made to measure the magnitude of this effect experimentally, and these measurements showed the worth of a single tube to decrease by 36% when all the adjacent void holes contained similar glass tubes. For the most heavily loaded case, as seen in Fig. 7.3, about two of every three available void holes contained a poison glass tube. No attempt has been made to correct the total measured worth of the distributed boron for these effects because of the variety of patterns that exist in the glass tube loading.

Even though the boron content within a single tube is small (2.9 wt-% B_2O_3), flux self-shielding within the glass cannot be neglected. The flux self-shielding factors were calculated with a cell calculation using transport theory.

The remainder of the analysis was made using ten broad groups and the full core radial diffusion-theory model used in the previous lattice assembly core analysis. The distributed boron was homogenized within the lattice core region with and without the self-shielding factors mentioned above. These data are shown on Table 7.6.

As shown, there is very good agreement between the calculated lumped and distributed poison worth. The calculated worth for the lumped poison was reported previously and was obtained by explicit transport-theory calculations. The "measured worth" is too low by 18% for the core with the single large rod and by 50% for the core with two large rods; where twice as many poison tubes are involved and the interaction effects are larger. Because of the good

Table 7.5
DISTRIBUTED POISON INVENTORY FOR EQUIVALENT
LARGE ROD CORE CONFIGURATION

Core Description	Outer Radius of Assembly (cm)			No. of 3-ft Poison Tubes in Lattice	Mass of Boron Added (g)
	HTGR Lattice	C/U-2500	Reflector		
<u>Core A:</u> No large control rods	28.84	69.82	80.76	0	0.0
<u>Core B:</u> One large control rod at center	28.84	77.43	88.57	176	78.0
<u>Core E:</u> Two large control rods separated	28.84	84.53	92.49	372	164.9

Table 7.6
CALCULATED AND MEASURED REACTIVITY WORTH FOR DISTRIBUTED POISON LOADINGS

Core Configuration	Reactivity Worth of Poison (\$)		Effective Core Multiplication (k_{eff})	
	Calculated	Measured	Calculated	Measured
Reference, no poison in lattice	0.0	0.0	1.024	1.014
One large rod, core and reflector added:				
Lumped poison	9.20	---	1.022	1.014
Distributed poison	9.00	7.60 ^a	1.020	1.012
Two large rods separated, core and reflector added:				
Lumped poison	14.90	---	1.011	1.014
Distributed poison	13.95	9.20 ^a	1.018	1.013

^aMeasured data not corrected for interaction effects in the measurement.

Fig. 7.3--Cross section of HTGR lattice assembly containing distributed poison loading equivalent to two large control rods separated by fuel element

agreement with the measured k_{eff} for each of the configurations, it has been concluded that the calculated worth of the distributed poison can be estimated to much better than 10%.

REACTIVITY WORTH OF HAFNIUM SAMPLES

Hafnium, which is made up of six different isotopes (each having different thermal and resonance characteristics) in the form of hafnium carbide has good stability under extremely high temperatures and thus has potential for use in HTGR systems. Experiments are being performed to measure the reactivity worth of various concentrations of HfC in different sized cylindrical containers to show that its worth can be adequately predicted.

All of the measurements with hafnium have been made at the center of lattice assembly No. 2. The initial series of experiments was made with loadings of hafnium carbide in a 7/16-in.-diameter thin-walled stainless steel tube that was loaded in the central void hole of the central lattice fuel element. These experiments will later be extended to include tube diameters up to 3.5 in.

The hafnium carbide was in the form of fine particles (less than 44 μm) and was blended with graphite to obtain dilute concentrations. The homogeneity of the diluted mixture was good. The hafnium carbide contained about 6 wt-% carbon and 2 wt-% zirconium, and the remainder was hafnium. The density of the hafnium within the tube ranged from 1.2 to 6.0 g/cm^3 , the latter value being the case in which no dilution was made. The density of the particle was determined to be about 12.1 g/cm^3 , which is very near the theoretical density of hafnium carbide.

The reactivity measurements were made using substitution methods with the total core excess reactivity adjusted by reflector addition. This is identical to the method used in the lumped burnable poison studies for boron rods as reported in GA-8860. Pertinent loading data for the hafnium samples is given in Table 7.7. The measured results are shown in Fig. 7.4.

Table 7.7
LOADING DATA FOR HAFNIUM CARBIDE SAMPLES

Sample Description	Radius (cm)	ρ_{Hf} (g/cm^3)	Mass Hafnium (g/6-ft tube)	Mass of Carbon Diluent (g/6-ft tube)
All HfC	0.541	5.86	955.0	0.0
2/3 HfC	0.541	3.64	593.4	97.2
1/5 HfC	0.541	1.16	173.0	162.0

Initial analyses to predict the reactivity worth for the hafnium samples were made using ten broad groups and the radial diffusion-theory model used for the analysis of the lumped boron rods. Effective hafnium cross sections

Fig. 7.4--Calculated and measured reactivity worth of hafnium samples in small rod versus concentration in rod

were obtained by averaging the natural hafnium cross section over the calculated average lattice assembly No. 2 flux spectrum. This, of course, neglects resonance self-shielding effects, resulting in too large a value for the effective broad-group cross sections. As shown on Fig. 7.4, the calculated reactivity worth is higher than that measured by about 45%.

When the resonances and heterogeneous nature of the rod are taken into account in determining broad-group cross sections, much improved but still deficient results are obtained, as shown on Fig. 7.4. The five isotopes of hafnium for which resonance data were available were considered explicitly in the GGC resonance calculation. The thermal self-shielding factors were calculated using transport theory. The hafnium samples were then homogenized into the lattice assembly.

Further calculations are presently under way using the GAROL code to determine the effective broad-group cross sections for hafnium in the lump. This code accounts for both spatial (two region) and energy self-shielding in the resonance treatment. A review of the cross-section data for hafnium is also under way.

Task VIII
PHYSICS AND FUEL MANAGEMENT

LOW-ENRICHMENT CYCLE STUDIES

Various studies (Refs. 1-3) have been conducted to determine the relative merits of using the $U^{235}/U^{238}/Pu$ (low-enrichment uranium) cycle in the HTGR. The results, when compared to the reference $U^{235}-U^{233}/Th/U^{233}$ (thorium) cycle have at times appeared attractive, at others not so attractive. These contradictions are generally attributable to the choice of physical condition such as power density, thermal conductivity, etc., and to the choice of economic conditions, such as the size of the HTGR economy (which influences fabrication and reprocessing cost parameters), cost of enriched uranium, working capital rates, etc. A study has been instituted with the intention of maintaining consistency and quantifying the relative merits of the two cycles in the HTGR.

The study is considering three stages. The first utilizes only existing technology, the fuel element design and design criteria being identical to those for the Fort St. Vrain reactor. In addition, maximum fuel ratings (in kw/ft) will be limited to the same values as in the Fort St. Vrain. The second stage of the study is also limited by existing technology but does permit the redesigning the fuel element to obtain more lumping or shielding of the U^{238} resonances, which is advantageous in the low-enrichment cycle. The third stage of this study is more optimistic, assuming improvements in particle and/or fuel rod materials and design to allow higher peak power densities and fuel ratings. Comparisons will be made with the reference cycle for each stage. Much of the work has now been completed on the first stage of this study and is discussed below.

The basic Fort St. Vrain fuel element has 108 coolant holes interspersed among 210 fuel holes arranged in a triangular array with a 0.74-in. pitch within a hexagonal graphite block. The fuel rods are composed of spherical UC_2 kernels, coated with successive layers of pyrocarbon and SiC. The particles are packed to a density of 64 vol-% and held together with a carbonaceous binder. Thus, within the fuel rod the ratio of carbon to U^{238} atom is about 30% as large as it is considering the entire fuel block. The resonance integral of the U^{238} is dependent on the size of the fuel particles, the concentration of U^{238} in the rod (depending on t/D , the ratio of fuel particle coating thickness to kernel diameter), the fuel rod diameter, and the closeness of adjacent rods (Dancoff factor). Table 8.1 demonstrates the relative importance of each of these parameters in reducing the U^{238} resonance integral, which in turn, reduces the required uranium enrichment for a constant ratio of C to U^{238} in the fuel block. The more sensitive parameters are the fuel rod diameter, U^{238} concentration in the fuel rod, and the Dancoff factor.

To efficiently perform a survey of equilibrium fuel cycles, it is convenient to fix the over-all C/ U^{238} atom ratio and the fuel residence time;

then for each set of fuel rod diameter and pitch, there is a unique set of fuel parameters. The GAFFE code (Ref. 4) is then used to search for the feed enrichment required to meet criticality at the end of the reload interval.

Table 8.1
EFFECT OF VARIOUS FUEL PARAMETERS ON U^{238} EPITHERMAL CROSS SECTION

Fuel Rod Diameter (in.)	t/D^a	Fuel Particle Diameter (μm)	Dancoff Shielding Factor	$\sigma_c^{U^{238}b}$
0.3	0.4	600	0.0	13.19
0.4	0.4	600	0.0	11.49
0.6	0.4	600	0.0	9.33
0.4	0.5	600	0.0	13.51
0.4	0.4	600	0.0	11.49
0.4	0.3	600	0.0	9.45
0.4	0.4	600	0.0	11.49
0.4	0.4	800	0.0	11.27
0.4	0.4	1000	0.0	11.00
0.4	0.4	600	0.0	11.49
0.4	0.4	600	0.24	10.14
0.4	0.4	600	0.36	9.31

^a t/D is the ratio of fuel particle coating thickness to kernel diameter.

^bAverage cross section above 2.38 ev, which is proportional to the resonance capture integral.

For stage 1 of this study, the C/U^{238} ratio was varied from 250 to 750 and the fuel residence times from three to five years. Using annual refueling, a fuel particle t/D of 0.35, and a fuel rod pitch of 0.74 in., the feed and discharge enrichments are as shown in Fig. 8.1. With a reasonable residence time, it is obviously not possible to reduce the enrichment much below 8% with the Fort St. Vrain fuel element design.

Fuel cycle costs have been calculated for these low-enrichment cycles using the typical (Ref. 5) cost parameters given in Table 8.2. The fuel cycle costs are shown in Fig. 8.2. Minimum costs are obtained with fuel residence times in excess of four years, with $C/U \approx 500$. Superimposed on the cost curves in Fig. 8.2 are the age-peaking factors. For the minimum cost point these are slightly in excess of 1.3 which is satisfactory to satisfy fuel temperature limitations.

These fuel cycle results indicate that the low-enrichment cycle is 0.3 to 0.5 mills/kw-hr higher than the thorium cycle in the HTGR. This is considerably different than the results of other analyses (Refs. 1, 2, and 3). These differences are being studied.

Fig. 8.1--Initial and final enrichments versus cycle time and C/U ratio

Fig. 8.2--Fuel cycle cost with recovery versus cycle time

Table 8.2
COST PARAMETERS FOR LOW-ENRICHMENT CYCLES

Preirradiation storage time,	0.5 year
Postirradiation storage time	0.5 year
Interest rate	10%
Ore cost	\$8.0/lb U_3O_8
Conversion cost ($U_3O_8 \rightarrow UF_6$)	\$2.70/kg U
Separative work cost	\$26/unit
Tails enrichment	0.2%
Plutonium value	\$9.5/g
Fuel particle fabrication cost	\$60/kg
Fuel element and assembly cost	\$1000/element
Recovery cost:	
Shipping	\$230/element
Reprocessing	\$65/kg U

POWER DISTRIBUTIONS

Most of the techniques used for minimizing power peaking in the HTGR were developed several years ago. In the intervening time the basic design of the reactor has changed significantly, and currently, the power shaping techniques are being reviewed.

The power distribution transverse to the coolant is influenced by two basic phenomena, the gross radial shape and the perturbations to this shape associated with the scattered region refueling scheme. The gross radial power shape is usually adjusted from the normal Bessel function shape, with a reflector peak by zoning the fuel, both fertile and fissile. Van Howe (Ref. 6) has shown that the fissile fuel may be zoned to establish the desired power shape while proper adjustment of the fertile materials will maintain the shape with respect to burnup. The result of using scatter loading is to present discontinuities in the macroscopic fission cross section at the interfaces between fuel regions of different aged fuel. These effects and possible gross flux tilting from the anisotropy of fuel age distribution which are superimposed on the gross radial shape are referred to as age-peaking and tilting, respectively.

One of the most significant changes that has occurred in the HTGR in the past few years is a continual move toward lighter loadings influenced primarily by economic considerations with private ownership of fuel. The result has been a significant softening in the nominal core neutron energy spectrum. With the earlier, harder spectra, the maximum power peak occurred at the core reflector interface because of the influx of neutrons from the cold, unpoisoned reflector. With the adoption of lighter fuel loadings and larger diameter cores, the importance of this reflector peak has been reduced.

With a uniform uranium and thorium loading, the basic flux shape with reflector peak still exists; however, for equilibrium conditions all radial regions have achieved burnup of about 0.7 fissions per initial fissile atom (fifa) and the center peak is diminished. The resulting gross radial power

distributions at several time points during an equilibrium year are shown in Fig. 8.3. Excluding the initial time point, the gross radial power peak remains below 1.15, which is comparable to the power peaks obtained with a zoned core. At the beginning of the cycle, the 1.25 peak would have to be controlled by the available control rods.

More work must be done, including two-dimensional calculations, which need not azimuthally average the distribution of fuel ages. In addition, methods of obtaining a flat distribution in the initial core must be developed and the effect of inserted control rods must be determined. At this stage, the unzoned core looks quite attractive; however, the unzoned power distribution is sensitive to relatively small perturbations and a considerable number of detailed calculations will be needed before zoning can be reduced.

The magnitude of the age peaking factor is dependent on the size of the refueling region and the degree of burnup. For a given burnup in a very small region, the flux spectrum and relative magnitude are determined essentially by the average composition of the surrounding fuel. The age peaking factor is then defined as the ratio of the one-group macroscopic fission cross section in the region relative to the average fission cross section of all ages of fuel. As the regions get larger, they tend to establish an internal flux shape and the definition of age-peaking becomes more complex. The maximum region size is dictated by both peaking and by reactivity control requirements, since the region also defines the boundaries of the control rod macrocell. The minimum region size is dictated by strength considerations in the top head of the PCRV and by the capital costs associated with each refueling penetration.

For the annual refueling with burnable poison, the reference region size (seven fuel columns) is about optimum, since the center and edge power peaks are about equal and sufficient shutdown margin is available.

With frequent or on-line refueling, control strength requirements are reduced because of the smaller requirements for excess reactivity. A study has been performed to determine the effect of changing the size of the refueling region on the age-peaking factor. The study consisted of a series of cell calculations in which fresh fuel was surrounded by average fuel and the results are shown in Fig. 8.4. With frequent or on-line refueling, minimum age-peaking (or cell peaking) factors are obtained with refueling regions whose equivalent radius is 45 cm or less. The seven-column refueling region has an equivalent radius of about 50 cm and introduces about 3% added cell peaking compared to the single column region. Most of this effect is due to not having burnable poison in the fresh fuel, since the principal fuel cycle advantage of frequent or on-line refueling is to avoid the neutron losses to burnable and control poisons. The burnable poison in the fresh fuel effectively reduces the tendency to produce a power peak at the center of the freshly refueled region.

XENON OSCILLATIONS

As reported previously (see earlier quarterly report GA-8662), the XENOSC code (Ref. 7) was adapted to the analysis of xenon transients in the HTGR.

Fig. 8.3--Radial power distribution versus radius

Fig. 8.4--Relative power in fresh fuel versus radius (beginning of cycle, monthly refueling, $C/Th = 250$, $\tau = 4$ years)

Additional modifications have now been completed that include methods of controlling rather than merely observing the transient. This program, GALAHAD, has been described in a topical report (Ref. 8).

When a xenon transient has been initiated, there are many possible choices of control rod configurations that could be used to damp the oscillation. The current version of GALAHAD has provisions for both input control sequences and program choices using optimization routines. The choices currently available are (1) the transient can be allowed to proceed with no control rods present in the reactor; (2) the control rods can be moved in the reactor to keep constant relative reactivity worths (the reactivity of each rod is maintained at the same value it held at the time the transient started); (3) all rods can be maintained at equal δk (the relative reactivity is equal for all rods); (4) rods can be inserted or withdrawn in specified groups (the order of the rod groups can be determined either by the local power peaking in the reactor or can be specified as input); (5) a linear program algorithm can be used to find the proper rods to insert or withdraw at each time step in order to minimize the power peaking at that time step; and (6) mixtures of the above strategies can be employed during different time intervals (labeled decision intervals). The GALAHAD code currently divides the period following the transient into four decision intervals. The number of time steps in each interval is determined by the input data.

The strategy to be used during each decision interval should be the one that finds the optimum value of some criterion measuring the severity of the entire transient. The criterion or objective function used by GALAHAD is the sum of the squares of the deviation in power from the average power at each mesh point in the reactor. These sums, which are similar to variances, are added together for each time step during the transient. The code determines the strategy that produces a minimum value for the total of these sums at all time steps. The process for finding the best strategy to be used during each decision interval is the dynamic programming algorithm of GALAHAD.

Figure 8.5(a) shows the square of the deviation of the local power from unity integrated over the reactor during a transient in which no corrective action is taken. This function is a converging oscillation with a time integral (objective) of 2223. Figure 8.5(b) shows the results of adjusting the rods by a constant δk per rod for each decision interval. The transient is quickly damped with the objective reduced to 1110. In Fig. 8.5(c) the rods are moved by banks in each decision interval, and the objective is 1627, primarily resulting from too little reactivity during the first part of the transient. Allowing the code to select the optimum form of control is shown in Fig. 8.5(d) and reduces the objective to 1036. However, as seen in Fig. 8.5(d), while this approach is advantageous during the early portion of the transient, a small oscillation persists due to continued control rod motion. Additional strategies are being investigated.

HTLTR SUPPORT ACTIVITIES

The High Temperature Lattice Test Reactor (HTLTR) at BNWL is playing an important role in the development of HTGR reactor physics technology. Gulf General Atomic has worked closely with the BNWL staff in the development of

Fig. 8.5--"Variance" in power shape during xenon oscillation

an experimental physics program that focuses on an area of major uncertainty in reactor physics, the high-temperature characteristics of HTGR type lattices.

The HTLTR facility is in the midst of an extended series of equipment shakedown tests. Also in progress is the development of experiment techniques along with some preliminary measurements with a typical HTGR uranium-thorium-graphite lattice.

The long-range program for HTGR-type lattices in the HTLTR has been tentatively set as follows:

Lattice	C/U ²³⁵	C/U ²³³	C/Th
1 (current)	6,000	0	200
2	0	11,000	200
3	11,000	0	300
4	(Unspecified)		

This program brackets the lattices of interest for advanced HTGRs. The U²³³ particles for lattice 2 are currently being received at BNWL from Oak Ridge. The lattice 2 loading will be made up and preliminary measurements will be made in the PCTR facility at BNWL before completion of the lattice 1 tests. The use of the PCTR for some of the preliminary room-temperature measurements should make use of the HTLTR more efficient.

The first lattice is currently being heated to about 300°C with measurements being made as a function of temperature for the cell reactivity, copper reactivity coefficient, vanadium reactivity coefficient, and the reactivity of some special poison cells (containing Sm, Eu, or Gd). Measurements of the neutron reaction rates in the cell and in the copper will be performed after cooldown to room temperature. Subsequently, following additional equipment checks and core adjustments, the lattice will be taken to near 1000°C.

Preliminary Analyses of HTLTR Lattices

To better understand the variations in the various parameters that will occur in the experiments and to look at some possible loading variations of future experiments, the following calculations were performed:

1. Spectrum calculations as a function of core temperature for the present HTLTR lattice loading.
2. Spectrum calculations as a function of core temperature for some possible variations in the U²³⁵ and thorium loadings, and for the specific loading being proposed as the next U²³⁵/thorium case.
3. Spectrum calculations as a function of core temperature for the proposed U²³³/thorium lattice in the HTLTR.

Lattice Configuration and Materials. Figure 8.6 is a schematic of the HTGR lattice part of the HTLTR assembly. Each graphite fuel block is loaded identically; the configuration of the fuel block is given in Table 8.3.

Fig. 8.6--HTGR lattice core (central region of HTLTR core)

Table 8.3
LATTICE GEOMETRY

Graphite block:

Thickness	3-3/4 in. square
Length	24 in.
Volume, including holes	5530.6 cm ³

Fuel holes:

No. of holes	25
Diameter	0.470 in.
Length	23 in.
Volume of 25 holes	1634 cm ³
Pitch, square array	3/4 in.

The fuel holes are filled with a homogeneous mixture of uranium, thorium, and graphite to give the desired C/Th and C/U ratios. The mixture is vibration packed into the 23-in.-long holes; threaded 1/2-in. graphite plugs seal the fuel holes at each end.

The U²³⁵ is in the form of coated particles of UC₂ with substrate diameters from 250 to 410 μm. These particles have been analyzed to have 67.94% U and 31.84% C; the enrichment is 92.92% U²³⁵. The thorium is in the form of ThO₂ particles with a size distribution up to 210 μm, but with half the weight in particles less than 44 μm. The U²³³ is in the form of coated particles of UO₂-ThO₂ with a Th:U weight ratio of 3 and a substrate size of 300 ± 70 μm.

Calculational Methods. The flux spectrum was obtained for the infinite lattice, both fast and thermal, using GGC-3 (Refs. 9, 10). The resonance calculation for thorium was done using the Nordheim integral method. Thermal self-shielding factors were calculated for the uranium particles (Ref. 11), and these were read directly into the thermal spectrum calculation. Both the thorium resonance calculation and the thermal spectrum calculation were done at several temperatures between 300° and 1200°K. The energy division between fast and thermal is 2.38 ev.

The definition for the infinite multiplication factor is:

$$k_{\infty} = \frac{\nu \Sigma_f \phi_1 + \nu \Sigma_f \phi_2}{\Sigma_{a1} \phi_1 + \Sigma_{a2} \phi_2},$$

where the subscripts 1 and 2 refer to the fast and slow neutron energy groups. The fast and thermal parts are coupled by $\phi_1 \Sigma_{out} = \phi_2 \Sigma_{a2}$; hence,

$$k_{\infty} = \frac{\eta f_2 + \frac{\nu \Sigma_{f1}}{\Sigma_{out}}}{1 + \frac{\Sigma_{a1}}{\Sigma_{out}}} = \eta f_2 P + \eta f_1,$$

where P is the probability of escape to thermal

$$= \frac{\Sigma_{\text{out}}}{\Sigma_{\text{out}} + \Sigma_{a_1}},$$

and

$$\eta f_1 = \frac{v \Sigma_{f_1}}{\Sigma_{\text{out}} + \Sigma_{a_1}},$$

$$\eta f_2 = \frac{v \Sigma_{f_2}}{\Sigma_{a_2}}.$$

The resonance escape probability for thorium is defined as

$$p = \frac{\Sigma_{\text{out}}}{\Sigma_{\text{out}} + \Sigma_{a_1}^{\text{th}}},$$

where

$$\Sigma_{a_1}^{\text{th}} \approx \frac{I}{\Delta u} \cdot N_{\text{th}}, \quad (\text{equality within a few percent})$$

$$I = \Sigma_i \sigma_i \Delta u_i,$$

with Δu the total lethargy range (15.65), N_{th} the thorium atom density in the block, and I the thorium resonance integral in the lattice.

For the temperature coefficient of reactivity,

$$\frac{\Delta \rho}{\Delta T} = \frac{k_2 - k_1}{k_1 k_2} \frac{1}{\Delta T} = \frac{1}{k} \frac{\Delta k}{\Delta T} = \frac{1}{p_{\text{th}}} \frac{dp_{\text{th}}}{dT} + \frac{1}{f} \frac{df}{dT} + \dots$$

$(1/p_{\text{th}})(dp_{\text{th}}/dT)$ represents the effect on the temperature coefficient of the change in thorium resonance absorption (and hence the thorium resonance escape probability p_{th}).

The relationship between p_{th} and I is

$$p_{\text{th}} = e^{-AI},$$

where A is a function of the macroscopic scattering of the graphite. If there is no change in the amount of graphite,

$$\frac{1}{p_{th}} \frac{dp_{th}}{dT} = \ln p_{th} \frac{1}{I} \frac{dI}{dT} \quad .$$

Variation of k_{∞} With Temperature and Loading. Some results of the spectrum calculations are shown in Table 8.4. Case 1 is the loading that is presently being measured in the HTLTR. These results were all obtained for a buckling of zero. A change in k_{∞} of only 0.003 was obtained when the buckling was increased to 0.0002, the geometric buckling of a 10-ft cube.

A comparison of cases 1 and 2 (Table 8.4) shows the effect of simply converting half of the lattice holes to void; there was an increase in the thorium resonance integral due to the change in the Dancoff factor. Reducing the uranium and thorium loading also softened the thermal-neutron spectrum. The effective boron capture cross section below 2.38 ev was 421.3 for case 1 and 496.5 for the reduced loading of case 2. Despite the softening of the thermal spectrum, the thermal utilization factor decreased from 0.686 to 0.655. This was due to the increased fractional absorption of carbon, whose mass was unchanged, which went from 3.0% of the total thermal absorptions in case 1 to 5.8% for the reduced uranium loading of case 2.

In case 3 the thorium was contained in half the holes at the same concentration, as was true of case 1; in case 4 it was contained in all the holes with half the concentration. These data show the sensitivity of the thorium resonance absorption to the concentration of thorium in the fuel holes for the same over-all C/Th and C/U ratios. There was a significant increase in the resonance integral in case 3 ($I = 37.9$ to $I = 41.7$) when the lump concentration was kept the same, but the Dancoff correction factor was reduced (half of the holes loaded); the increase was even larger in case 4 (to $I = 48.6$), when all the holes were loaded with half of the lump concentration.

For those two cases where the lump concentration was the same (cases 1 and 2), the value of $(1/I)(dI/dT)$ was nearly the same even though the thorium loading was different by a factor of two. However, the Doppler coefficient $(1/p_{th})(dp_{th}/dt)$ was quite different, -8.1×10^{-5} versus -4.1×10^{-5} . Cases 3 and 4 have the same C/Th ratio, on the other hand, and the value of $(1/I)(dI/dT)$ was different, but the Doppler coefficient was essentially the same.

A comparison of cases 2 and 3 indicates that the Doppler coefficient is a function of the C/Th ratio and independent of the C/U ratio. This conclusion has also been substantiated by the results obtained at the HTGR critical facility (see earlier quarterly progress report GA-8662).

The sensitivity of the k_{∞} value in case 1 to variations in the mass inventory is given below:

Increase uranium content 5%	+0.017 Δk_{∞}
Increase thorium content 5%	-0.026 Δk_{∞}
No particle self shielding of uranium	+0.048 Δk_{∞}
Increase carbon content 5%	+0.007 Δk_{∞}

Task IX

ADVANCED COATED PARTICLE FUELS

DILUENT PARTICLE DEVELOPMENT

Uranium-235 fuel in HTGR coated fissile particles is presently diluted with thorium for the initial core loading to restrict fuel burnup and to provide a prompt negative temperature coefficient. In recycle reactor cores, however, the presence of thorium in the fissile particle may not be required to provide the negative temperature coefficient because of the presence of bred U^{233} with the thorium in the fertile particles. From a fuel cycle cost standpoint, thorium is not an ideal diluent for restricting burnup because the U^{235} breeds U^{236} (a nuclear poison), which cannot be economically separated from the thorium-bred U^{233} . Therefore, in order to avoid the neutron penalty associated with the U^{236} , it is desirable to substitute other diluents for thorium in the fissile particles. Such a substitution should ultimately lead to a reduction in HTGR fuel cycle costs. Therefore, work is under way to evaluate the possibility of substituting some other diluent for thorium.

Void and carbon are of greatest initial interest, because they would provide dilution with no sacrifice in neutron economy. Void volume would apparently be the nearest thing to an ideal diluent in that it would provide extra space for fission products in addition to restricting fuel burnup. This should enable the coated particle fuel to be taken to higher burnups before reaching the design buildup of fission-gas pressure. The use of diluent particles might also allow higher fuel loadings, thereby providing more freedom in fuel element design.

A mathematical-model study was conducted to evaluate the influence of certain significant parameters on irradiation-induced coating stresses and diametral changes in buffer-diluted coated particles. This was a comparatively brief parametric study using both TRISO (Ref. 1) and BISO models. The study was intended to provide aid in designing coated-particle samples for irradiation in capsule P13L.

The input parameters that were investigated include PyC and buffer thickness, PyC density, and irradiation temperature. The remaining parameters shown in Table 9.1 were held constant.

Some of the results for BISO-coated particles irradiated at 1200°C are plotted in Figs. 9.1 and 9.2. Figure 9.1 shows the influence of increasing the PyC thickness with constant buffer thickness (50 μm), and Fig. 9.2 shows the influence of increasing buffer thickness with constant total coating thickness (120 μm). Maximum stress is reduced more by increasing the buffer thickness than by increasing the PyC thickness. In Fig. 9.1, increasing the PyC thickness with a constant 50 μm buffer even causes the maximum stress to increase, particularly when a PyC density of 1.87 g/cm³ is used. This is

Fig. 9.1--Influence of PyC thickness on calculated maximum BISO coating stress, maximum shrinkage, and end-of-life dimensional change. Buffer thickness was held constant at 50 μM . Calculations based on HTI PyC data at an irradiation temperature of 1200°C

Fig. 9.2--Influence of buffer thickness on calculated maximum BISO coating stress, maximum shrinkage, and end-of-life dimensional change. Total coating thickness was held constant at $120 \mu\text{m}$ (i.e., increases in buffer thickness accompanied by equal decreases in PyC thickness). Calculations based on HTI pyrocarbon data at an irradiation temperature of 1200°C

because irradiation-induced radial expansion in a thick PyC spherical coating causes high tensile stress at the outer surface at the end of life.

Table 9.1

CONSTANT INPUT PARAMETERS USED IN BISO AND TRISO CALCULATIONS

Kernel type	UC ₂
Kernel diameter, μm	100
Kernel density, g/cm^3	10.8
Buffer density, g/cm^3	1.1
Theoretical buffer density, g/cm^3	2.2
PyC irradiation-induced creep constant, (10^{21} psi n/cm ²) ⁻¹	2.25 at 1050°C 3.0 at 1200°C
PyC Young's modulus, 10^6 psi	3.5
PyC Poisson's ratio	0.17
SiC Young's modulus, 10^6 psi	54 at 1050°C 52 at 1200°C
SiC Poisson's ratio	0.18
End-of-life burnup, % FIMA	75
End-of-life fast-flux exposure, 10^{21} n/cm ²	8
Irradiation-induced dimensional change in PyC	HTI data (Ref. 2)

NOTE: Burnup dose relationship was assumed to be parabolic (decreasing burnup rate with increasing dose) to a dose of 6×10^{21} n/cm². Burnup was then held constant at 75% FIMA.

Increasing the buffer thickness also tends to eliminate end-of-life expansion; however, maximum shrinkage increases slowly with increasing buffer thickness. A PyC density of 1.87 g/cm^3 appears to be more desirable than 1.79 g/cm^3 , because of the lower shrinkage during life and the less pronounced tendency to expand at end of life. The density of 1.87 g/cm^3 , however, requires at least $80 \mu\text{m}$ of buffer to limit maximum PyC stress below 20,000 psi.

A similar study was done using a programmed irradiation temperature rather than a constant irradiation temperature. The study was intended to account for the temperature remaining constant at 1200°C to a fast dose of 1.5×10^{21} n/cm², at which point the temperature drops to 1050°C and remains there until the end of life. The modification of the program to account for a change in temperature makes it necessary to provide additional materials parameters. Apparently, the most important of these parameters are irradiation-induced dimensional change and creep. There have been indications that

dimensional change is more rapid at a given temperature when the sample has been previously irradiated at a higher temperature. An attempt was made to establish a dimensional-change - fast-dose relationship after the temperature change by assuming a curve between the 1200° and 1050°C curves. The creep constant for 1200°C was used up to the temperature change, and that for 1050°C was used thereafter. When programmed temperatures were used with the assumed curves, stress levels were higher than those for constant temperatures of either 1050° or 1200°C due to the use of the lower creep constant with the intermediate dimensional change data. The trends, however, were the same as those mentioned above.

The TRISO parametric study was intended to evaluate PyC densities of 1.79 and 1.87 g/cm³, inner PyC thicknesses of 15 and 20 μm, outer PyC thicknesses of 20 and 30 μm, and buffer thicknesses of 50 and 60 μm.

The most severe stress condition occurred early in life during the rapid initial densification of the inner PyC layer. These stresses exceeded 20,000 psi in every case considered. The best combination of parameters considered was 100 μm core, 50 μm buffer, 15 μm inner PyC, 20 μm SiC, and 20 μm outer PyC, with both PyC layers having a density of 1.87 g/cm³. The maximum stress calculated for this particle was 24,000 psi. The data indicated that a slightly higher density for the inner layer would reduce this stress.

COATED PARTICLE IRRADIATIONS

Capsule P13K

The final burnup analyses have been completed for the particles irradiated in capsule P13K and are shown in Table 9.2. The results show that the diluent particles in cells one through four achieved burnups ranging between 50% and 59% FIMA. The burnups in cell five were determined to be 44% FIMA. Three thorium-diluted particle batches were tested for a reference, and these samples reached burnups between 16% and 22% FIMA. In most cases, the burnup values were higher than the design value of about 50% FIMA; however, the majority of the samples did survive the test, which is very encouraging.

It should be noted that for samples 3742-123E and 3663-143E, which were tested at both 900° and 1300°C, the burnups for the lower-temperature particles were significantly higher than for the high-temperature particles. This result may be a contributing factor to the higher failure percentages observed for the lower-temperature batches (see Table 9.2).

The postirradiation metallographic and stereographic examination of these particles were discussed in detail in the previous quarterly report (GA-9090).

Capsule P13L

Capsule P13L will be a follow-on test of coated diluent carbide particles. Three samples of coated oxide particles will also be tested. The samples will be irradiated at 1500°, 1300°, and 900°C to 6 to 8 x 10²¹ n/cm². The design burnups are 75% FIMA for diluent fissile particles and 20% FIMA for conventional fissile particles.

Table 3.2
DESCRIPTION OF COATED PARTICLES TESTED IN CAPSULE P13K
(Three Cycle KTR Irradiation)

Lot No.	Coating							Particle				Uranium Loading (g U/cm Coated Particles)	Irradiation Conditions						Postirradiation Examination		Remarks
	Type ^a	Buffer		Isotropic			Total Thickness (μm)						Burnup		Fissions/cm ² Coated Particle (x 10 ²⁰)	Temperature (°C)			Fast Dose (10 ²¹ nvc)	Macro Exam (X Fail)	
		Thickness (μm)	Density (g/cm ³)	Thickness (μm)	Density (g/cm ³)	Anisotropy Factor		X PDMA	Avg	Max	NOL										
													Type	Size (μm)		Density ^b (X T.D.)	Th:U Ratio				
3742-123E	BISO-LTI	45	0.89	28	1.94	1.0 ^c	73	UC ₂	88-105	97	—	0.69	50	9.0	1280	1355	1140	2.7	3	2	Buffer diluent series
3662-7E	BISO-LTI	42	0.85	45	1.94	1.0 ^c	87	UC ₂	88-105	97	—	0.50	57	7.3	1255 ^d	1350	1060 ^d	2.9	0.2	0.4	
3742-149E	BISO-LTI	45	0.98	25	1.94	1.0 ^c	90	UC ₂	88-105	97	—	0.47	59	7.1	1260 ^d	1370	1060 ^d	2.9	0.3	5	
3742-131E	BISO-LTI	58	0.98	38	1.92	1.01	96	UC ₂	88-105	97	—	0.42	58	6.3	1220	1355	1080 ^d	2.9	0.2 ^e	0	
3742-139E	BISO-HTI	45	0.89	35	1.97	1.00	80	UC ₂	88-105	97	—	0.59	51	7.6	1270 ^d	1370	1160 ^d	2.7	30 ^e	23	
3663-143E	BISO-LTI	48	0.87	44	1.81	1.00	92	UC ₂	150-250	57	—	0.90	44	10.0	1225 ^d	1300	1175	2.2	0	0.3	Void diluent series
3663-141E	BISO-LTI	60	0.95	64	1.86	1.01	124	UC ₂	150-250	57	—	0.58	53	7.9	1305 ^d	1330	1275 ^d	2.8	0	0	
3663-141E	BISO-LTI	60	0.95	64	1.86	1.01	124	UC ₂	150-250	57	—	0.58	43	6.4	1295 ^d	1360	1225 ^d	2.2	0	0.6	
3663-137E	BISO-LTI	85	1.04	68	1.82	1.03	153	UC ₂	150-250	57	—	0.40	54	5.5	1310	1360	1260	2.8	0	0	
3923-17E	BISO-HTI	62	0.98	61	1.83	0.99	123	UC ₂	150-250	57	—	0.58	51	7.7	1250 ^d	1350	1130 ^d	2.7	27 ^e	44	
3663-37E	BISO-LTI	62	1.12	53	1.91	1.02	115	(Th,U)C ₂	150-250	93	1.6	0.34	22	4.7	1310 ^d	1345 ^d	1280 ^d	2.8	0	0	Thorium diluent series
3662-9E ^f	BISO-HTI	53	1.26	91	2.01	1.04	144	(Th,U)C ₂	150-250	93	1.6	0.23	16	2.6	1260 ^d	1336 ^d	1206 ^d	2.2	0	0	
3742-123E	BISO-LTI	45	0.89	28	1.94	1.0 ^c	73	UC ₂	88-105	97	—	0.69	57	10.4	855 ^d	900	820 ^d	2.7	5	7	Lower temperature series, buffer diluent
3663-143E	BISO-LTI	48	0.87	44	1.81	1.00	92	UC ₂	150-250	57	—	0.90	56	12.8	905 ^d	960	870 ^d	2.7	3	8	Void diluent
3663-37E	BISO-LTI	62	1.12	53	1.91	1.02	115	(Th,U)C ₂	150-250	93	1.6	0.34	21	4.6	855 ^d	920	820 ^d	2.7	0.1	0	Thorium diluent

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

^b Percent of theoretical density. High-density particles are spheroidal; low-density particles are irregularly shaped.

^c Estimated.

^d Thermocouple failed during test.

^e Significant number of coated particles stuck in the graphite thimble.

^f Doped with Si.

The preparation and evaluation of the samples for capsule P13L have been completed (Ref. 3). The samples that were selected for insertion in the capsule are listed in Table 9.3. As shown in the table, there are four basic sample series. In all cases except the fertile particles, buffer dilution was achieved by placing a 50- to 60- μm -thick buffer on the 100- μm (nominal) diameter UC_2 cores. The fertile particles are of the large type (600- μm diameter) envisioned for use in recycle cores (they are not diluent particles).

The four basic sample series are:

1. Oxide Series. These samples are of interest from the recycle fuel standpoint. The UO_2 and ThO_2 was obtained from ORNL and coated at Gulf General Atomic.
2. Conservative Design Series. These samples have standard TRISO coatings on 100- μm (nominal) diameter cores. The result is a coating design that is quite conservative.
3. Fail-early Design. This sample was designed by selecting and depositing coatings that would result in early failure as determined by the Kaae model (Ref. 1). The coatings were essentially in accordance with standard criteria, except for a low-density inner isotropic layer. A design that would fail late in life was attempted, but none within the framework of standard design thicknesses was found.
4. Repeat Sample. A sample that was prepared for capsule P21 was included for comparison.

Capsule fabrication is in progress; insertion in the ETR is scheduled for April (cycle 102).

References

1. Kaae, J. L., "A Mathematical Model for Calculating Stresses in a Four-layer Carbon-silicon-carbide-coated Fuel Particle," J. Nucl. Mater., to be published; also Gulf General Atomic report GA-9031, October 14, 1968.
2. Bokros, J. C., G. L. Guthrie, R. W. Dunlap, and A. S. Schwartz, "Radiation-induced Dimensional Changes and Creep in Carbonaceous Materials," J. Nucl. Mater., to be published; also USAEC report GA-8700, Gulf General Atomic, August 30, 1968.
3. Akins, R. J., Coated Particles Used in the P13L Irradiation Experiment, USAEC report GAMD-9169, Gulf General Atomic, to be published.

Table 9.3
COATED PARTICLES TO BE IRRADIATED IN CAPSULE P13L

Batch No.	Material Type	Core Type	Coating Class	Overcoat Type ^a	Planned Irrad. Temp (°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
4000-889	Fissile	(Th,U)C ₂	TRISO	LTI	900,1300,1500	P21 repeat
4413-79	Fertile	ThO ₂	BISO	LTI	1300	Oxide 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

^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.

Task XI
GRAPHITE RESEARCH

CONTROL ROD MATERIALS IRRADIATION STUDIES

The purpose of the control rod materials irradiation experiments is to determine the relative importance of the potential damage mechanisms leading to dimensional change in graphite containing large amounts of boron (0.4 to 0.5 g B/cm³) in the form of B₄C granules. The specimens (0.58 in. by 0.20 in. by 0.20 in., and 0.58 in. by 0.40 in. by 0.20 in.) were irradiated for four cycles between April 18, 1968 and November 1, 1968 in an instrumented capsule (BG-1) in the ETR. The fast-neutron dose was 3 to 4 x 10²¹ n/cm² (E > 0.18 Mev) in 2736 hr at temperatures of 500° to 700°C. This exposure is equivalent to that received in three to four years of HTGR operation.

During the quarter, capsule BG-1 was returned to Gulf General Atomic, and postirradiation examination was initiated.

The material and the specimen configurations have been described in a previous quarterly report (GA-8356). All of the specimens were removed from the capsule without damage and showed no change in visual appearance as a result of the irradiation. In subsequent transfer operations, one sample out of the total of 68 was inadvertently broken into two pieces.

There were either two or three samples in each of 28 graphite crucibles. The typical radiation level of the samples contained in a single crucible was 600 mR beta and 150 mR gamma at 2 in. These could be handled outside the hot cell in the subsequent examination. Two of the 13 materials included in the experiment showed excessive radioactivity (15 R gamma at 2 in.), the source of which was traced to Ta¹⁸². The boron addition in these materials had contained 2000 ppm Ta as an impurity. Dimensional change measurements and metallography of these samples will be conducted in the hot cell.

Samples of material were scraped from the specimens on the surface exposed to thermal neutrons and from surfaces shielded from thermal neutrons. The samples will be subjected to B¹⁰/B¹¹ isotopic analysis to determine the extent of B¹⁰ depletion and the B¹⁰ burnout gradient that developed in the specimens during irradiation.

Appendix A

DESIGN CRITERIA DC-21-1

REHEAT STEAM DRIVEN HELIUM CIRCULATOR

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DESIGN CRITERIA

DC-21-1

REHEAT STEAM TURBINE DRIVEN HELIUM CIRCULATOR

BASE PROGRAM, TASK I

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REHEAT STEAM TURBINE DRIVEN HELIUM CIRCULATOR			
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<p>1. <u>SCOPE</u></p> <p>This document contains design criteria for reheat series 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 circulator 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 after it leaves the high pressure element of the main steam turbine and before it enters the reheater. 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>			
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4. AERODYNAMIC PERFORMANCE CRITERIA

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.

Table 1

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

Flow rate (lb/sec)	284
Inlet pressure (psia)	821
Inlet temperature (°F)	724
Outlet pressure (psia)	615

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 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.

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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.

5.4 In addition to the normal operating loads, the thrust bearing must withstand loads due to the following accident conditions:

- a. Turbine exhaust pipe burst 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.

B Shutdown seal actuation must be prevented by instrumentation interlock during shaft rotation.

5.7 Each circulator is required to have a mechanical brake which must be capable of stopping the rotor rotation from 5% of the full load speed down to zero speed.

B Rotor brake actuation must be prevented by instrumentation interlock above 5% speed.

6. DRAINAGE & LEAKAGE

B 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.

B 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.

B 6.3 A limited amount of steam is allowed to leak from the steam passages into the circulator. Essentially no leakage is allowed from the circulator into the steam channels while the steam turbine is operational.

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e. Compressor inlet temperature and pressure.

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REHEAT STEAM TURBINE DRIVEN HELIUM CIRCULATOR

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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.

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 B

DESIGN CRITERIA DC-1-1

BASE CASE DESIGN PARAMETERS

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4	APPLICABLE CODES	4
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1. SCOPE

This design criteria is applicable to all plant structures and foundations with the exception of Prestressed Concrete Reactor Vessels and Secondary Containment Structures.

2. LOADS AND FORCES

2.1 WIND LOADS:

2.1.1 The magnitude of pressure or force on structures subjected to wind load shall be calculated using the methods described in the American Society of Civil Engineers (ASCE) Paper No. 3269, "Wind Forces on Structures."

2.1.2 Design Wind. The basic wind for design purposes shall be 100 mph wind acting 30 feet above grade. The variation of wind velocity with elevation shall conform to the recommendations of ASCE Paper No. 3269. A gust factor of 1.1 shall be applied to the design wind.

2.1.3 Tornado. The design of the plant shall be such as to withstand a tornado of 300 mph horizontal vortex wind velocity plus a 60 mph translational velocity (360 mph total) allowing the safe shutdown of the plant. Those items necessary for the safe shutdown of the plant are defined as Class I items. A total static pressure drop of 3 psi in 3 seconds shall be assumed to occur during the passage of the tornado. This pressure drop does not occur concurrent with the tornado wind.

Class I items are defined as those structures or equipment items necessary for a safe shutdown of the plant and whose failure or loss of function might cause uncontrolled release of radioactivity. Structures which support Class I equipment items or whose failure could damage such items are also treated as Class I items.

2.1.4 Missile Protection. The Class I structures and equipment items shall be protected from missiles generated by the tornado wind before and after occurrence of the static pressure drop.

The missile generated by the tornado is selected to be a fir plank traveling at 200 mph. The plank is 4 in by 12 in by 12 ft long and has a weight of 105 lbs.

Class I structures and equipment items shall also be protected from missiles generated from failure of rotating equipment such as main turbine, feedpumps, etc., and from failure of tendons or tendon anchors.

2.2 SEISMIC FORCES:

2.2.1 Class I Items. Seismic forces acting on Class I structures and equipment items shall be obtained by dynamic analysis (see paragraph 3.2). The basic ground acceleration of the bedrock for the "design earthquake" shall be 0.16 g horizontal coincident with a 0.11 g vertical. For "maximum earthquake," basic ground acceleration of the bedrock shall be assumed to be 0.25 g horizontal coincidental with a 0.17 vertical.

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The values of damping shall be those of Table I.

"Design Earthquake" is defined as the strongest probable earthquake expected at the site in the future. Class I structures and equipment items shall remain in the elastic range and shall perform the required functions during and after the earthquake.

"Maximum Earthquake" is defined as the maximum credible earthquake at the site. The Class I structures and equipment items shall maintain their function allowing safe shutdown of the plant. Moderate plastic yielding of steel and concrete cracking is permitted.

2.2.2 Other than Class I Items. The seismic forces acting on plant structures other than Class I items may be obtained by the same methods prescribed for Class I items or by the procedures specified in the Uniform Building Code (1967), using Seismic Zone 2 parameters.

2.3 CRANE LOADS:

Crane and trolley loads shall conform to the requirements of the American Institute of Steel Construction Specification for the Design, Fabrication and Erection of Structural Steel for Buildings, AISC 1963.

2.4 ROOF LOADS:

The roof shall be designed for dead load plus a uniformly distributed live load as defined in the Uniform Building Code, 1967, or the snow load, whichever is greater. Special provisions shall be made in roof areas where an abrupt change of elevation between adjacent roof levels may produce an excessive accumulation of snow.

In addition to the specified live and dead loads, steel trusses shall be designed to support a construction load of 2000 pounds assumed to act at any one panel point of the lower chord.

2.5 FLOOR LOADS:

Floors shall be designed to support the concentrated loads, impact loads, equipment weights or uniformly distributed live loads as set out in approved plant arrangement drawings. Where loads are not otherwise defined, the load provisions of Chapter 23 of the Uniform Building Code, 1967 Edition, shall be applied.

Equipment weights and configurations shall be obtained from approved manufacturer's drawings supplied with the building layout drawings. If such information is not available, an equivalent floor load should be supplied. Design for impact and support stiffness under machine loads shall follow manufacturer's recommendations. If design recommendations are not available, the mass of the equipment foundation shall be a minimum of 2-1/2 times the weight of the equipment. Composite action between the machine foundation and floor may be considered in arriving at the required foundation mass and rigidity. Possible resonance between equipment and foundations shall be considered in the design.

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TABLE I**
DAMPING VALUES

Stress Level	Type and Condition of Structure	Percentage of Critical Damping
1. Low, well below proportional limit, stresses below 1/4 yield point	a. Vital piping	0.5
	b. Steel, reinf. or prestr. conc., wood; no cracking, no joint slip	1.0
2. Working stress, no more than about 1/2 yield point	a. Vital piping	1.0
	b. Welded steel, prestr. concr., well reinf. concr. (only slight cracking)	2
	c. Reinf. concr. with considerable cracking	5
	d. Bolted and/or riveted steel, wood structs with nailed or bolted joints	5
3. At or just below yield point	a. Vital piping	2
	b. Welded steel, prestr. concr. (without complete loss in prestress)	5
	c. Prestr. concr. with no prestress left	7
	d. Reinf. concr.	7
	e. Bolted and/or riveted steel, wood structs, with bolted joints	10
	f. Wood structs with nailed joints	15
4. Beyond yield point, with permanent strain greater than yield point limit strain	a. Piping	5
	b. Welded steel	7
	c. Prestr. conc., reinf. conc.	10
	d. Bolted and/or riveted steel, or wood structs	20
5. All ranges	Rocking of Entire Structure	
	a. On rock, $c > 6000$ fps	2
	b. On firm soil, $c \geq 2000$ fps	5
	c. On soft soil, $c < 2000$ fps	7

** From N. M. Newmark "Design Criteria for Nuclear Reactors Subjected to Earthquake Hazards," 25 May 1967.

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2.6 FOUNDATION LOADS:

Ground water pressures shall be determined as the hydrostatic water pressures. The highest ground water level shall be assumed to be 5 ft 0 inches below grade.

Earth pressures acting against foundation walls shall be determined as "rest pressures" producing lateral pressures equivalent to 50% of the respective vertical pressures. The possibility of the development of passive earth pressure during a seismic disturbance must also be considered for foundations supporting or protecting Class I items.

2.7 OTHER FORCES:

Structures subjected to impact, vibration, to temperature changes and temperature gradients and other extraordinary conditions shall be designed with due regard for such conditions.

3. ANALYTICAL METHODS

3.1 Structural response due to dead load, live load, snow and wind and temperature shall be analyzed statically employing rational analysis. Elastic or plastic design methods may be used at the discretion of the designer.

3.2 Structural response of Class I items due to earthquake motions shall be determined by dynamic analysis of a representative analytical model. The response spectra approach or time dependent analyses using actual or synthetic earthquake motion records may be used.

If the response spectra approach is employed, the response spectra for the Design Earthquake shall be those average response spectra of Figures 1.19 through 1.24 of TID-7024 (Nuclear Reactors and Earthquakes, August 1963) adjusted to the ground acceleration specified in paragraph 2.2. (See Figures 1 and 2) The maximum response shall be determined by the methods of modal superposition described in TID-7024, Chapter 1, paragraph 1.3B.

If a time dependent dynamic analysis is performed, the motions used shall be such that the response spectra for those motions will be at least equal to the smoothed response spectra values specified in Figures 1.19 through 1.24 of TID 7024.

For Maximum Earthquake, similar analytical approaches as used for the Design Earthquake may be used. Alternately, the structures may be analyzed by methods accounting for the elasto-plastic energy absorption characteristics of the structure.

4. APPLICABLE CODES

4.1 Concrete structures made of reinforced, prestressed, composite or precast concrete, shall be designed to conform to the requirements of the "Building Code Requirements for Reinforced Concrete ACI-318-63, except as noted otherwise in paragraph 5.

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4.2 Steel structures shall be designed to conform to the requirements of the 1963 AISC Specification for the "Design, Fabrication and Erection of Structural Steel for Building."

4.3 Structural elements made of other materials shall be designed to conform to the applicable provisions of the latest edition of the Uniform Building Code except as otherwise noted in paragraph 5.

5. STRESS ALLOWABLES

Loading Combination	Steel	Concrete
1. DL+LL } 2. DL+LL+W _D }	AISC allowable stresses or load factors, including provisions for allowable stress increases for wind.	ACI-318-63 allowable stresses or load factors, including provision for allowable stress increases for wind.
3. DL+LL+E	<u>Class I Structures:</u> AISC allowable stresses but without allowance of a 33% allowable stress increase. For plastic design E shall be considered as LL. <u>Other than Class I Structures:</u> AISC allowable stresses	ACI-318-63 allowable stresses but without allowance of a 33% allowable stress increase. For ultimate strength, or plastic design E shall be considered as LL.
	(i.e. increased allowable stresses)	
4. DL+LL+E' } 5. DL+LL+W _T }	10% capacity reserve for buckling members and members designed by plastic analysis or 1.67 of AISC allowable stresses. Deflections shall not inhibit safe plant shutdown.	10% capacity reserve for buckling members and members designed by ultimate strength methods, or 1.67 of AISC allowable stresses. Deflections shall not inhibit plant shutdown.

Key: D.L. = Dead Load	W _T = Tornado (Wind)	*For Class I Structures only.
L.L. = Live Load	E = Design earthquake	
W _D = Design Wind	E' = Maximum earthquake	

Where significant thermal stresses occur effecting the structural safety of the element under consideration, they shall be added to the DL, LL, and wind stresses.

6. FOUNDATION CONDITIONS

6.1 The soil profiles for the site show alluvial soil and rock fill to a depth of 8 ft. 0 in.; Brassfield limestone to a depth of 30 ft.; blue weathered shale and fossiliferous Richmond limestone to a depth of 50 ft. 0 in.

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6.2 Allowable soil bearing in 6,000 psf. Allowable rock bearing on Brassfield limestone and Richmond limestone in 18,000 psf and 15,000 psf respectively.

Allowable bearing on bedrock is 50,000 psf.

These allowable bearing stresses shall be maintained for combinations of Dead Load, Live Load, Design Earthquake and Design Wind. For load combinations, considering maximum earthquake or tornado, these allowable bearing stresses may be exceeded by 67%.

The foundations shall be designed such that settlements occurring under any combination of Dead Load, Live Load, Design Wind, or Design Earthquake, will not adversely effect continuous plant operation.

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AVERAGE RESPONSE SPECTRA FOR EARTHQUAKE GROUND MOTION
16 %g GROUND ACCELERATION

FIGURE 1

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AVERAGE RESPONSE SPECTRA FOR EARTHQUAKE GROUND MOTION
25 %g GROUND ACCELERATION

FIGURE 2

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Appendix C

DESIGN CRITERIA DC-70

STRUCTURES

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BASE CASE DESIGN PARAMETERS

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8	Turbine Plant	4
9	Overall Plant Output	5

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9. Overall Plant Output

a. Gross generator output Mw	1016.5
b. Auxiliary electrical power, Mw	16.5
c. Net station output (to main power transformer), Mw	1000.0
d. Net station heat rate, Btu/kw-hr	8,663
e. Net station efficiency, %	39.39

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