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HTGR GENERIC TECHNOLOGY PROGRAM SAFETY, SYSTEMS AND COMPONENT DESIGN AND DEVELOPMENT

**QUARTERLY PROGRESS REPORT
FOR THE PERIOD ENDING
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ABSTRACT

This publication continues the two quarterly report series on HTGR Component and Systems Development and HTGR Safety. The tasks previously covered (safety and reliability, structural materials, thermal barrier, and reactor internals) are now part of the HTGR Generic Technology Program, which also includes heat exchanger engineering and mechanical and electrical engineering reported here. The structural materials work will be reported next month. The primary purpose of the HTGR Generic Technology Program is to develop, in a time frame consistent with application to specific HTGR plants, the HTGR technology base and design definition of nuclear components and systems which are common to HTGR reactor types.

The work documented here includes HTGR safety and safety-related studies to better understand and more accurately define safety characteristics and safety margins of GCRs under postulated accident conditions. Also included are the design, analysis, and testing of the PCRV, liner, penetrations, thermal barrier, reactor internals, steam generator, CAHE, and rotating machinery. Design studies and analysis plus experimental procedures and results are discussed and, where appropriate, the data are presented in tables, graphs, and photographs. More detailed descriptions of experimental work are presented in topical reports.

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INTRODUCTION

This report covers the work performed by the General Atomic Company under four program areas of the High-Temperature Gas-Cooled Reactor (HTGR) Generic Technology Program sponsored by the U.S. Department of Energy under Contract EY-76-C-03-0167, Project Agreement No. 65. This Project Agreement calls for development of the HTGR technology base and design definition of nuclear components and systems which are common to HTGR reactor types.

Characteristics of large HTGR designs include:

1. A single-phase gas coolant allowing generation of high-temperature, high-pressure steam with consequent high-efficiency energy conversion and low thermal discharge.
2. A prestressed concrete reactor vessel (PCRV) offering advantages in field construction, primary system integrity, and stressed member inspectability.
3. Graphite core material assuring high-temperature structural strength, large temperature safety margins, and good neutron economy.
4. Thorium fuel cycle (with high or medium enriched uranium) which allows good utilization of nuclear resources and minimum demands on separative work.

These basic features are incorporated into the 330-MW(e) prototype Fort St. Vrain reactor which is currently undergoing startup testing.

Work under the program areas of HTGR safety and reliability, structural engineering, heat exchanger engineering, and mechanical and electrical engineering are included in this report.

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1. SAFETY AND RELIABILITY (DV&S)
189a NO. 00790; PROGRAM AREA 1

1.1. INTRODUCTION AND SUMMARY

The main objectives of this work area are to ensure that HTGR generic design features meet applicable safety and licensing criteria, and to develop safety reliability and availability data for HTGR components and systems. In FY-78 generic activities have mainly focused on Accident Initiation and Progression Analysis (AIPA), Safety Research, Safety and Reliability Computer Code Development, Nuclear Steam Supply (NSS) Systems Analysis, and NSS Systems Methods Development.

Some major milestones scheduled for this quarter have been accomplished. Volume IX of the AIPA status report was completed in its final form and presented for management review. The report will be issued in the near future.

Project work statements (PWSs) for a cooperative GCR safety program between GA and KFA of Germany under DOE/BMFT auspices were reviewed. The objective was to agree upon PWSs for submittal to the joint Ad Hoc Committee in April 1978. The PWSs included subtasks of AIPA, accident analysis, and graphite chemistry.

The CAR program status report for FY-77 was internally reviewed and is being prepared for final publication. Work was completed on acoustic and vibrational analysis in the primary loop which extends probability-of-exceedance calculation methods to a collection of similar components such as thermal barrier assemblies.

1.2. AIPA METHODS DEVELOPMENT

The primary objectives of the AIPA study are to provide guidance for safety research and development programs for HTGRs by means of probabilistic risk assessment (PRA) and to obtain risk information necessary to license the HTGR on its own merits.

In FY-78 the main work efforts include preparation and issue of Volume IX of the AIPA status report, safety studies of the effects of major containment fires, and evaluation of how core heatup sequences are terminated.

1.2.1. AIPA Volume IX Status Report

The effort this quarter has been mainly to issue Volume IX, which contains the results of the AIPA study since publication of the first eight volumes (Ref. 1-1). A large part of Volume IX is devoted to the Phase II core heatup study, which reflects the knowledge gained through the implementation of R&D recommendations that were derived from Phase I results. Volume IX also includes detailed analyses of accident sequences initiated by steam generator and reheater tube leaks, as well as a discussion of NRC, KFA (Germany), SRD (United Kingdom), and other external review comments that have been made on the AIPA results previously presented.

1.2.1.1. Summary and Main Conclusions of AIPA Volume IX

Earlier accident studies only analyzed a representative set of accident sequences to accomplish the more narrowly defined objectives of Phase I. The potential licensing applications of the Phase II study required that a comprehensive assessment of HTGR risks be provided. Hence the scope of the Phase II study was extended to consider a much broader range of accident sequences in terms of both an increased number of initiating events and a wider spectrum of plant responses to core heatup transients. Whereas only a single representative initiating event for transients leading to core

heatup was analyzed in Phase I, the Phase II study includes about twenty new initiating events to provide an overall assessment of the probability of core heatup at about 3×10^{-5} /reactor-year.

A broader range of plant responses to core heatup has been considered in Phase II as a result of improvements in the accident simulation models obtained during a 2-year study on PCRV integrity which permit a simulation of the long-term transient response of the plant structures including the PCRV and containment building. Analysis of the long-term accident effects includes detailed thermal and structural analysis of the core and PCRV out to long times on the order of 240 hours into the event, which is unusual in the safety evaluation of any reactor concept. A total of about 160 accident sequences have been identified, analyzed, and categorized in terms of 22 different core heatup scenarios. In addition, new experiments have been initiated, some of which have been completed. A new radionuclide transport program has been used to predict the time- and temperature-dependent transport and plateout of iodine. This model predicts a wide range of plateout factors depending on the particular core heatup scenario. However, more work, both theoretical and experimental, remains to be done in this area.

In terms of the magnitude of radiological consequences, a total of six release categories have been identified that span the spectrum of 160 accident sequences and 22 plant scenarios. The methodology for the quantification of uncertainties in the prediction of both accident probability and consequences has been improved and applied in a more rigorous fashion relying more on Monte Carlo techniques and less on analytical approaches that require simplifying assumptions, the effects of which are difficult to quantify. The reliability data base employed has been expanded primarily in the areas of gas-cooled reactor steam generators and circulators, as well as emergency diesel generators. With these improvements, the Phase II results represent a comprehensive assessment of public risks due to HTGR accidents.

The overall results for the dominant accident sequences are presented in Fig. 1-1 together with the Phase I/Interim results from Volume VIII. The abscissa in Fig. 1-1, the so-called "health effects dose," is a linear combination of the whole body gamma and inhalation organ doses at 2500 meters, which is proportional to the total number of latent cancer fatalities for the representative U.S. site assumed in the study (approximately 0.13 latent cancer fatality per health effects rem). Each point in Fig. 1-1 represents the median point estimate of the consequence level and the total frequency of all accident sequences having similar consequences (i.e., the frequencies are those of the entire release category which has contributions from numerous accident sequences). It is understandable that the Phase II results (solid symbols) indicate generally higher levels of risk than the Phase I/Interim results (shaded entries). This is attributed in part to an overall assessment as opposed to the analysis of representative sequences in Phase I. The existence of relatively high frequency core heatup sequences in Phase II (CH-5 and CH-6) is the result of the consideration of a spectrum of initiating events leading to core heatup. The highest consequence core heatup sequence identified in the Phase I/Interim analysis is initiated by a severe earthquake which prevents isolation of the containment (denoted as 1.5E-S). The highest consequence release category identified in Phase II is denoted as CH-1 at about 35 health effects rem and, like the earthquake sequence, involves failure to close the containment isolation valves. The difference in consequences predicted for CH-1 and 1.5E-S is the result of larger fission product release from the core before PCRV depressurization in the case of CH-1.

Core heatup release categories CH-2, CH-3, CH-4, and CH-5 involve decomposition of PCRV concrete and eventual overpressurization of the containment building on the order of days into the transient by one of two causes. Categories CH-2 and CH-4 involve burning of combustible gas mixtures, i.e., water gas ($H_2 + CO$), which are produced when water vapor liberated from PCRV concrete reacts with core graphite at high temperature. In categories CH-3 and CH-5 the necessary conditions for water gas burning

Fig. 1-1. Overall HTGR risk assessment in terms of frequency and consequence of reheater leak and core heatup release categories

are not reached; however, a sufficient quantity of noncondensable gases is liberated from the concrete to gradually overpressurize the containment, which generally occurs later in time than the case of water gas burning. The maximum risk category, i.e., that having the highest product of frequency and consequence, is CH-5 which is also the highest probability core heatup category involving containment failure. It is shown later that, except at very low or very high frequencies, CH-5 also dominates the overall risk curve expressed in terms of the complementary cumulative distribution function, i.e., the probability per reactor-year of exceeding a given level of consequence.

Also indicated in Fig. 1-1 are revisions to the Phase I results recommended by KFA as a result of their extensive review of the first seven volumes of the AIPA report series. Although KFA recommends an increase beyond the Phase I envelope, the increases are bounded by the more comprehensive assessments presented in Volume IX.

An overall assessment of HTGR accident risks consistent with the 1975 reference HTGR design* assumed in the study is presented in the form of a complementary cumulative distribution curve on the health effects dose in Fig. 1-2. Also shown are individual curves for each of the core heatup release categories and the dominant reheater leak category to indicate the relative contributions of each to the overall assessment. Category CH-5, which was shown to be the highest risk category in Fig. 1-1, is seen to dominate the overall assessment in the range of 1 to 100 rem and gives way to category CH-3 beyond 100 rem. The curves in Fig. 1-2 reflect a probabilistic statement of the uncertainties in the consequence assessments and assume median (best estimate) values of the frequencies predicted for the accident sequences.

*An alternative HTGR design currently being evaluated appears to offer improved safety features over that assessed in the AIPA study by virtue of a decreased core power density and elimination of gas reheat.

Fig. 1-2. Complementary cumulative distribution of health effects dose - overall assessment and contributions from release categories

It should be noted that the refinements incorporated into the Phase II event trees have not yet been used to provide a reassessment of the risks associated with large earthquakes. It is believed, however, that the consequences of core heatup sequences initiated by large earthquakes would be no greater than CH-1, and the median estimates of the probabilities would be too small to result in any significant impact to the overall assessment curve in Fig. 1-2 even if the probability values recommended by KFA were employed (see Fig. 1-1). On the other hand, the large uncertainties associated with the predictions of earthquake occurrence rates and resultant equipment failure probabilities clearly warrant further work in this field.

An overall assessment of health effects and property damage impact at frequency levels of 10^{-6} and 10^{-7} per reactor-year provided by the Phase II results is summarized in Table 1-1. Despite the differences indicated in Fig. 1-1, the conclusions derived from the Phase I results are substantially unchanged. There is a change, however, in the risk of latent health effects such as cancer fatality. Phase II results predict an increase beyond Phase I by an amount proportional to that indicated in Fig. 1-2 although the magnitudes are low in absolute terms when compared to normal incidence rates. For example, the total number of cancer fatalities in the population at risk to the accidents analyzed in this study, i.e., about 850,000 people over a period of 30 years, would be, assuming a normal incidence rate of 1.7×10^{-3} /person-year, a value of over 43,000. Therefore, at the 10^{-7} /per reactor-year frequency level, the impact of a postulated HTGR accident would be an increase in the incidence of cancer fatality of less than 0.03% for the representative U.S. site assumed in the study. It should also be mentioned that the latent cancer assessment is based on the linear (no threshold) hypothesis discussed in the BEIR report which is viewed by many as conservative.

Although the results at both 10^{-6} and 10^{-7} per reactor-year indicate property damage costs of approximately \$1 million, these are associated with the cost of evacuation only, which is assumed to be carried out even though evacuation would have only a minor impact on the resultant health

TABLE 1-1
HEALTH EFFECTS AND PROPERTY DAMAGE AT FREQUENCY
LEVELS OF 10^{-6} AND 10^{-7} PER REACTOR-YEAR

Accident Consequences	Frequency (reactor-year ⁻¹)	
	10^{-6}	10^{-7}
Early fatalities	<1	<1
Early illnesses	<1	<1
Property damage	<\$1 million	\$1 million
Relocation area (square miles)	0	0
Decontamination area (square miles)	0	0.2
Latent cancer fatalities ^(a)	1	8
Thyroid nodules ^(b)	10	100
Genetic effects	<1	1

(a) BEIR commission recommendations used; linear hypothesis used with no dose rate effectiveness factor reduction.

(b) Sum of benign and cancerous nodules.

effects. The effects of fallout on the land surrounding the plant would be insufficient to require relocation of the nearby population at either the 10^{-6} or 10^{-7} level of accident frequency and only at 10^{-7} would the fallout be sufficient to warrant decontamination of approximately 130 acres immediately surrounding the plant. The low magnitude of property damage impact assessed for the HTGR suggests advantages in nuclear accident insurance.

Topics discussed in Volume IX in addition to the results of the Phase II study include the peer review that has been performed subsequent to Volume VIII and the issue of whether comparisons between the results of the AIPA study and the Reactor Safety Study are valid.

An extensive review of Volumes I through VIII has been performed by KFA as part of an HTGR risk assessment study of comparable funding to the AIPA study. Comments that resulted from this review, together with those provided by Dr. E. V. Gilby of the Systems Reliability Directorate, United Kingdom Atomic Energy Authority, and Dr. W. E. Vesely of the U.S. Nuclear Regulator Commission, are discussed in Volume IX. These comments are viewed by the AIPA study team as predominantly favorable, as well as necessary to focus on important areas for continued development of the evolving methodology of risk assessment.

Possible future applications of the AIPA results in the area of HTGR licensing may require that comparisons be made with the results of the Reactor Safety Study on LWRs. The validity of such comparisons, in view of the fact that the AIPA study does not have the benefit of knowing the "as-built" details of the plant and that some differences may exist in the details of the respective methodologies employed in the two studies, has been examined. A comparison of the computer models used to estimate accident consequences in the two studies shows that comparable results are obtained for given radionuclide source terms. It is concluded that,

although comparability cannot be rigorously demonstrated because of inherent differences between the two reactor systems, order of magnitude comparisons between the results of the two studies are valid if suitably qualified. One such qualification is that the AIPA results should be viewed as "prospective" as opposed to the Reactor Safety Study results that are viewed as "retrospective."

Although probabilistic risk assessment has proved to be a valuable tool in bringing about an improved understanding of HTGR safety issues, much remains to be learned as the AIPA study continues. It is apparent from examination of Fig. 1-2 that the ranges of uncertainty associated with both the frequency and consequence predictions are large and that continuation of experimental and theoretical safety research to investigate and an attempt to reduce these uncertainties is justified. Substantial progress has been made in Phase II to improve the theoretical treatment of radio-nuclide transport and plateout phenomena, and significant reductions in the consequence uncertainties seem possible in the near future as information from the planned plateout testing program becomes available. Additional safety research topics planned for the near future include improvements in the models and experimental data base used for predicting the behavior of the core during core heatup, including fuel particle failure, fission product release, and reactivity effects associated with material redistribution. It is planned to continue the Containment Atmosphere Response Program with emphasis on the response to core heatup events and, in particular, on fission product transport phenomena.

A study is in progress to provide an assessment of the risks associated with large electrical fires such as that which occurred at Browns Ferry. Although indications are that severe earthquakes do not make major contributions to the overall risk assessment, large uncertainties continue to exist in the prediction of earthquake occurrence rates and the resultant earthquake damage probabilities. Another subject under consideration for additional study is the extent to which the low risk predicted for HTGR accidents could be influenced by acts of terrorism and sabotage. A separate study is

being conducted at General Atomic for the U.S. Arms Control and Disarmament Agency, which involves an assessment of the societal risks associated with nuclear weapons proliferation from power reactor fuel cycles. It is expected that the insights and methodology used to assess proliferation risk may be useful in possible future efforts in the assessment of sabotage and terrorism.

An immediate objective in the continuation of the AIPA study is to explore how the results obtained thus far can be used to support the licensing process for future HTGRs. Potential applications include use of event trees in accident analyses performed for safety analysis reports, the selection of assumptions to be used for the maximum hypothetical fission product release siting analysis, and evaluation of the design protection against events involving anticipated transients with failure to scram (ATWS). Attendant to these efforts will be an expanded use of PRA as a tool to evaluate the suitability of various design options which affect the overall safety of the plant as well as to select the design basis events. The fact that probabilistic methods have been and continue to be applied to the HTGR in an early stage of its commercial development should contribute to further improvements in the safety margins of future nuclear power reactors.

1.2.1.2. Uncertainty Analysis in Radiological Dose Calculations

The central point estimates of radiological doses have in the past been obtained by employing a series of thermal and fission product transport computer programs and using median best estimate values for the input variables. However, it is desirable to know how the calculated dose can deviate from the central point estimate as input variables assume values different from their median values. In AIPA Phase I, this problem was handled by simply rerunning the series of computer programs, varying one parameter at a time, and observing which parameters had the largest effect on the dose. Dose deviations were then combined analytically by summing the deviations over all variables, using the logarithmic form of the law of propagation of errors.

A different approach was taken in the Interim Phase (AIPA Status Report Volume VIII). Each organ dose was assumed to be a multiplicative function of a small number of major independent variables, and the function was normalized to the central point estimate dose (determined by the series of computer runs) for median values of the variables. By Monte Carlo simulation of the variation of the parameters within their assigned probability distributions, the uncertainty distribution of each organ dose prediction was established, both in terms of the density (pdf) and cumulative (CDF) distribution forms. The CDF describes the percentiles of the dose uncertainty distribution which is easily converted to the complementary CDF, which is simply the complement of the CDF. For a given event sequence frequency, the complementary CDF expresses the conditional probability that the dose will be greater than a specified value. By multiplying the complementary CDF by the median event sequence frequency, cumulative dose curves are obtained describing the frequency or probability per reactor-year of exceeding a specified dose level.

There were several limitations in the interim approach. One is that the dose is not always a simple product of the independent variables. Another is that the various organ doses are dependent on a set of variables, some of which are common and some of which are independent of the remaining organ doses. Thus, the dose distributions are interrelated and not separate functions. Since health effects for each dose distribution were calculated and summed, this coupling effect was not rigorously accounted for.

In the Phase II analyses, these limitations were obviated by deriving a set of nonlinear equations in which the various doses are expressed as simultaneous functions of a common set of variables. Rather than rely on normalized linear scaling of doses from parameter derivations, the doses are derived from first principles. A subroutine to solve these equations for each set of parameter values was then incorporated into the STADIC Monte Carlo computer program (Ref. 1-2). Based on this set of equations, and sampling input probability distributions of the variables which affect

the consequences, the STADIC code calculates the interrelated organ dose distributions, the complementary CDFs, and the cumulative dose curves for each major event scenario. The present approach is considered to be a significant advancement over previous uncertainty analysis. A description of the equations is presented below, followed by a summary of the derived uncertainty distributions of the input variables and the STADIC code results and conclusions.

1.2.1.2.1. Equations. The dose equations are based on time-dependent gas-borne concentrations and leak rates for 12 selected radionuclides in the PCRV (q_p) and the containment (q_c) and the integrated radioactivity release to the atmosphere (Q_a). The radionuclides selected as dominant contributors to the dose are shown in Table 1-2. For simplicity in the equations, the following approximations are made:

1. An instantaneous release from the core to the primary coolant is assumed to occur at t_o . This is the time at which a similar dose calculated by the more rigorous TDAC computer program would result if the radionuclides were all released from the core at once. In general, t_o varies from nuclide to nuclide and is on the order of tens of hours for good accuracy. Values of t_o were derived based on comparisons with TDAC results.
2. The gasborne concentrations in the PCRV and containment and integrated release to the atmosphere were evaluated in four time intervals, where retention constants (λ 's) and leak rates (γ 's) were held constant over the intervals but changed if necessary from interval to interval.
3. Decay and buildup of daughter products were neglected after release from the core.

TABLE 1-2
RADIONUCLIDES AND DOSES CONSIDERED IN UNCERTAINTY ANALYSIS

Organ	Index J	Radionuclides	Dose Effectivities (rem/Curie)	Initial Inventory (Curies)
Whole body gamma	1	Xe-133	0.045	1.6×10^8
	2	Xe-135	0.26	8.0×10^7
	3	Kr-88	2.03	1.2×10^8
	4	Xe-133m	0.23	4.8×10^5
Inhalation thyroid	5	I-131	1.49×10^6	8.0×10^7
	6	I-133	3.95×10^5	1.6×10^8
	7	I-132	5.29×10^4	1.15×10^8
Inhalation lung	8	Te-132	3.17×10^4	1.1×10^8
	9	I-131	2.09×10^4	8.0×10^7
	10	Cs-134	5.12×10^5	1.4×10^7
	11	Cs-137	4.49×10^5	7.3×10^6
Inhalation bone	12	Sr-90	1.11×10^7	6.7×10^6
	13	Cs-137	6.13×10^4	7.3×10^6
	14	Te-132	3.22×10^3	1.1×10^8
	15	Sr-89	4.17×10^5	1.2×10^8

With these assumptions, the gasborne concentration equations for a specific radionuclide are as follows (nomenclature list is given in Table 1-3):

Release from Core. The release from the core is given by

$$q_{p,o} = f_p f_d Q_o \quad . \quad (1-1)$$

Since this is the activity released to the primary coolant that remains gasborne and available for release to the containment, it includes any plateout retention in the PCRV via the constant f_p . For noble gases, $f_p = 1$; for condensable nuclides, $f_p \leq 1$. Any holdup due to adsorption/desorption within the PCRV is accounted for in the f_d term, where $f_d = \exp(-\lambda_d t_o)$. Since t_o is input, it is chosen such that it represents the time of availability for release to the containment.

PCRV Concentration. Over an interval $(t_o - t_i)$ with constant PCRV egress rate γ_p , the time-dependent concentration in the primary coolant is

$$q_p = q_{p,i} e^{-A(t-t_i)} \quad . \quad (1-2)$$

Containment Concentration. The differential equation for the containment activity is

$$\frac{dq_c}{dt} = \gamma_p q_p - B q_c \quad , \quad (1-3)$$

with the boundary condition that $q_c = q_{c,i}$ at $t = t_i$; that is, the concentration is known and not necessarily zero at the start of the interval. For constant parameters, the solution over an interval is

$$q_c = \frac{q_{p,i} \gamma_p}{B - A} \left[e^{-A(t-t_i)} - e^{-B(t-t_i)} \right] + q_{c,i} e^{-B(t-t_i)} \quad . \quad (1-4)$$

TABLE 1-3
NOMENCLATURE LIST OF STADIC SUBROUTINE EQUATIONS

A	= $\lambda_r + \gamma_p$ (h^{-1}), total removal constant for fission products in PCRV
B	= $\lambda_r + \lambda_p + \lambda_c + \gamma_c$ (h^{-1}), total removal constant for fission products in containment
b	= breathing rate (m^3/s)
D	= radiological dose at 2500 m (rem) subscripts: WB = external whole body gamma exposure thy = inhalation thyroid exposure bone = inhalation bone exposure lung = inhalation lung exposure
E	= dose effectivity, rem/Curie
f_d	= $\exp(-\lambda_d t_o)$, decay or buildup before core release
f_p	= PCRV non-plateout release factor
q	= radioactivity of a specific fission product nuclide, Curies subscripts: p = in PCRV c = in containment a = in atmosphere i = at start of an interval o = at time zero
Q	= integrated radioactivity of a specific fission product nuclide, Curies Subscripts: o = in fuel at time zero a = released to atmosphere at time t i = released to atmosphere at start of an interval f = remaining in core at time of containment failure
t	= time, h subscripts: o = for release to primary coolant i = at start of an interval f = at containment failure
X/Q	= atmospheric dispersion factor, s/m^3
γ	= leak rate, vol/h subscripts: p = from PCRV c = from containment
λ	= exponential removal constant, h^{-1} subscripts: d = effective decay or buildup before core release p = plateout in containment c = filter cleanup in containment r = radioactive decay after core release

Depending on the relative importance of the terms, this equation predicts monotonically increasing, monotonically decreasing, or peak concentration in the time interval.

Atmospheric Release. Over an interval, the integrated release to the atmosphere is

$$Q_a = \int_{t_i}^t \gamma_c q_c dt \quad , \quad (1-5)$$

where $Q_a = Q_{a,i}$ at $t = t_i$. For constant parameters, the solution is

$$Q_a = \frac{\gamma_c \gamma_p q_{p,i}}{AB} - \frac{\gamma_c \gamma_p q_{p,i} e^{-A(t-t_i)}}{A(B-A)} + \frac{\gamma_c \gamma_p q_{p,i} e^{-B(t-t_i)}}{B(B-A)} + \frac{\gamma_c q_{c,i}}{B} \left(1 - e^{-B(t-t_i)} \right) \quad . \quad (1-6)$$

This is a monotonically increasing function which levels off with time and units of Curies.

The foregoing equations express the fission product concentration and release of a single nuclide to the atmosphere in a specific time interval in which the parameters such as the removal constant (λ) and leak rates (γ) remain constant. The accident is broken up into four intervals. The first interval extends from time t_0 to the input time of top head failure in which PCRV and containment leak rates are at their low value before concrete degradation. In the second interval, from the time of top head failure to initiation of side wall concrete degradation, the PCRV egress rate is increased to that value for top head concrete degradation only. This interval is roughly 30 to 40 hours for typical scenarios with concrete degradation. The third interval extends from the time of side wall degradation to the calculated time of containment failure. In this third interval, the PCRV egress rate is increased to its maximum rate. Thus, the end point of

this interval is an important dependent variable calculated from the set of independent variable values. The fourth interval extends from the time of containment failure to 720 hours. In this last interval, any instantaneous release at the time of containment failure is calculated. The containment leak rate in interval four is the calculated higher post-failure leak rate.

If j is a subscript denoting each of the 15 nuclides listed in Table 1-2 and k is a subscript denoting each of the four time intervals, the total integrated release to the atmosphere for each nuclide is

$$\sum_{k=1}^4 (Q_{a,j})_k \quad .$$

The external whole body dose is then determined by

$$D_{WB\gamma} = \frac{1}{4} (X/Q) \sum_{j=1}^4 \sum_{k=1}^4 (Q_{a,j})_k E_j \quad . \quad (1-7)$$

Similarly, the inhalation thyroid, lung, and bone dose equations are, respectively,

$$D_{thy} = b(X/Q) \sum_{j=5}^7 \sum_{k=1}^4 (Q_{a,j})_k E_j \quad (1-8)$$

$$D_{lung} = b(X/Q) \sum_{j=8}^{11} \sum_{k=1}^4 (Q_{a,j})_k E_j \quad (1-9)$$

$$D_{bone} = b(X/Q) \sum_{j=12}^{15} \sum_{k=1}^4 (Q_{a,j})_k E_j \quad (1-10)$$

For a given set of independent variables, the STADIC code solves the above equations to determine dependent variables and the four organ doses (whole body gamma, inhalation thyroid, inhalation lung, inhalation bone) as well as the health effects dose. Through repeated Monte Carlo trials,

the code derives the simultaneous distributions for the doses listed above. Independent variables are used to calculate the dependent variables, such as containment failure time, fraction of water reacting with core graphite in PCRV, and PCRV and containment egress rates after concrete degradation. The latter are calculated from the concrete spalling rate, considering the moles of gas liberated per unit time divided by the total moles in the system. The failure time of the containment is calculated from first principles.

1.2.1.2.2. STADIC Code Results. The spectrum of values which the independent variables can reasonably assume were analyzed as a fraction of core heatup scenario and nuclide group. Deviations which were considered were those due to normal material variations, random variations due to plant and weather conditions at the time of the accident, and the ability of the analyst to predict fission product transport and barrier failure phenomena.

A scenario represents certain plant accident response sequences created by an initiating event which contain similarities in the way of release and timing of fission products. From several such scenarios analyzed, the results from scenario J (CH-5) are given here since CH-5 presents one of the highest risk categories. Scenario J (CH-5) begins with a loss of main loop cooling assuming cooldown of main loops is successful for a while. Automatic trip of the reactor is accomplished and the reserve shutdown system therefore is not needed in this sequence. The core auxiliary cooling system is required to continue the core cooling at about 5 hours but does not start. Furthermore, restoration of forced circulation is not achieved either by repairing the main loops or the auxiliary loops in time. Therefore, there is a loss of forced circulation and core damage. The PCRV liner cooling system is assumed to fail at 114 hours, which leads to concrete damage and the release of decomposition gases. The containment had been isolated at or before 35 hours when the PCRV underwent blowdown and the containment cleanup system was brought into operation. However, the decomposition gases accumulate in the containment and eventually at about 300 hours cause containment overpressurization.

Figure 1-3 illustrates for scenario J (CH-5) the four organ dose distributions obtained with the STADIC computer program. The health effects dose distribution was then combined with the median scenario frequency to obtain the cumulative health effects dose for the various scenarios. These curves are shown in Fig. 1-2. This was done by multiplying the median frequency by $1 - P$, where P is the percentile of the cumulative probability distribution of the health effects dose distribution shown, for example, in Fig. 1-2. These cumulative health effects dose curves are used to define the Phase II core heatup risk envelope (Section 1.2.1.1). Complementary cumulative distribution curves of the amount of activity released to the atmosphere are shown in Fig. 1-4 for the release of Sr-90, Cs-134, I-131, and Xe-133 during scenario J (CH-5). Only small fractions of Sr-90 and I-131 release are calculated, due to plateout retention in the PCRV and containment. However, for Xe-133 no retention occurs and the curie release is determined largely by radioactive decay before containment failure.

1.3. HTGR SAFETY RESEARCH

The objective of the HTGR Safety Research Task is to examine and improve models used to describe HTGR behavior during accident conditions. The current areas of R&D were determined by the AIPA studies. These areas include (1) core material redistribution, (2) PCRV plateout, and (3) containment atmosphere response.

Analysis of all fission product release data obtained in the fuel particle heatup tests has been completed; the final report is 50% complete. PCRV plateout test data have been obtained in new tests of iodine adsorption on mild steel from 600 to 1100 K, from which rates of formation of FeI_2 will be derived. Application of the PADLOC computer program has been made to core heatup conditions with the helium shutoff valves open, allowing iodine plateout on steam generator surfaces. The calculations indicate much lower PCRV escape fractions than previously estimated for helium shutoff valves closed, since a large fraction of iodine in each pass plates

Fig. 1-3. Complementary cumulative distribution of doses at 2500 m given occurrence of scenario J (CH-5)

Fig. 1-4. Complementary cumulative distribution curves for curie release of key nuclides to atmosphere, scenario J (CH-5)

out irreversibly in the steam generator. The report on methods for predicting the chemical response of the containment atmosphere to accidents has been printed and will be issued shortly.

1.3.1. Core Material Redistribution

The analyses of all fission product release data have been completed. During this quarter, the cesium, tellurium, and iodine data obtained from the room-temperature mullite and charcoal traps were analyzed.

Comparisons between the xenon and krypton release data were made. These comparisons demonstrate, in terms of the fission product release model (Ref. 1-3), that:

1. The low-temperature and high-temperature components of the f_{β} curve (Ref. 1-3) intersect at the same temperature for xenon and krypton nuclides.
2. The transport parameters in the low-temperature region depend on the fission gas element.
3. The transport parameters in the high-temperature region are independent of the fission gas element.

These observations, in conjunction with other aspects of the analyses, indicate, respectively, that:

1. The parameter f_{β} depends primarily on the structure of the fuel kernel.
2. The fission gases move as single atoms or small clusters in the fuel for the low-temperature region.
3. The fission gases move as bubbles in the fuel for the high-temperature region.

The semiempirical fractional release function used in the fission product release model (Ref. 1-3) has been shown to correspond practically exactly to a release function derivable by solving the diffusion-trapping equation (Ref. 1-4). This correspondence has been demonstrated for the low-temperature region of the loss of forced cooling (LOFC) test data.

Preparation of the draft final report is about 50% complete.

1.3.2. PCRV Plateout

1.3.2.1. Plateout Tests

The behavior of iodine adsorption on mild steel, as well as the reaction with metal constituents (primarily iron), under core heatup accident conditions has been further investigated. The data from nine experiments are presented in Table 1-4. From the data it is evident that at high iodine vapor pressures (>30 Pa), and specimen temperatures down to 600 K, complete loss of iodine occurs with the majority of the iodine converting to FeI_2 . A relatively insignificant fraction of iodine is adsorbed as elemental iodine. It is shown that the rates of FeI_2 formation vary significantly with the test conditions. Detailed analysis of the reaction rates will be performed when more data are obtained. It should also be noted that metal-iodide formation is observed at iodine source vapor pressures down to 10^{-2} Pa (10^{-7} atm). The conversion to metal-iodide is still significant compared to the total adsorbed amount under these conditions. Further analysis of these data is in progress.

1.3.2.2. Plateout Analysis

The PADLOC computer program was further developed to enable calculation of iodine plateout in the PCRV during AIPA core heatup scenarios with the main loop helium shutoff valves open. A flow model through the steam generator cavity was developed. Node spacing was found to be important to assure perfect conservation of mass, with fine nodal detail needed at

TABLE 1-4
IODINE ADSORPTION DATA FOR AS-RECEIVED MILD STEEL

Iodine Source		Specimen Temperature (K)	Iodine Pressure Over Specimen (Pa)		Iodine Loading, True Area ^(a) ($\mu\text{g}/\text{cm}^2$)	Total I ₂ Consumed (mg)	Test Duration (h)
Temperature (K)	Pressure (Pa)		P _{I₂}	P _I			
297	3.70×10^1	1100	2.15×10^1	9.96×10^1	2.57×10^{-1}	165.95	47
297	3.70×10^1	600	5.25×10^1	2.76×10^{-1}	(c)	190.25	9.5
297	3.70×10^1	1100	2.15×10^1	9.96×10^1	1.56×10^{-2}	189.03	49
253	4.49×10^{-1}	600	6.76×10^{-1}	3.04×10^{-2}	(c)	66.46	406
253	4.49×10^{-1}	1000	8.92×10^{-2}	1.61	6.85×10^{-1}	9.59	96
253	4.49×10^{-1}	1100	4.1×10^{-2}	1.78	5.21×10^{-2}	16.94	209
253	4.49×10^{-1}	700	6.26×10^{-1}	2.41×10^{-1}	3.05	46.97	408
233	3.41×10^{-2}	1100	1.01×10^{-3}	1.46×10^{-1}	7.63×10^{-3}	0.411	264
233	3.41×10^{-2}	1000	2.14×10^{-3}	1.37×10^{-1}	1.1×10^{-2}	6.305	1010

(a) Roughness factor = 4.5.

(b) FeI₂ quantitatively analyzed and accounted for.

(c) Reading did not appear valid.

the top of the steam generator. Other input-output programming changes were made to facilitate smooth calculation of these scenarios. Results showed that iodine behaves as a perfect sink on passage through the steam generator section and that PCRV escape fractions are dominated by the flow partition between the PCRV release train and the steam generator cavity. Significantly lower escape fractions were determined compared to scenarios with the helium shutoff valves closed.

1.3.3. Containment Atmosphere Response (CAR) Program

The CAR Program status report covering FY-77 work has been printed (Ref. 1-5), and is awaiting DOE patent clearance. This report describes the methods and techniques developed to analyze the chemical response of the containment atmosphere to accidents which may potentially affect the structural integrity of the containment building.

Work during the present quarter was performed to expand the CNTB computer program capability to include the computation of the different processes followed by iodine fission products entering the containment atmosphere during unrestricted core heatup (AIPA accident study). These processes are:

1. Diffusivity and Retentivity of Stressed Concrete. With the exception of the containment floor, which is chemically treated, the concrete surfaces exposed to the containment atmosphere are able to absorb and retain iodine. These two processes have been experimentally and analytically described at North Carolina State University for Brookhaven National Laboratory (Ref. 1-6).
2. Sorption by Coated Steel Surfaces. The sorption of iodine is intentionally promoted on epoxy coated surfaces exposed to the containment atmosphere. Coated surfaces are the containment liner and the side plate covers of the PCRV. Part of the piping and other exposed steel surfaces may also be coated. Mass

transfer coefficients for iodine (given by Ref. 1-7 for illustrative purposes) are presently being computed on the basis of the iodine diffusivity through the containment atmosphere.

3. Filtration of the Containment Atmosphere. The iodine removed by the filters in the circulating portion of the containment atmosphere is determined by a more complete model than the one existing in the TDAC computer program (Ref. 1-8).
4. Steam Condensation. Steam condensation provides a perfect sink for iodine, since it is soluble in the water drained to the containment sumps. This perfect sink behavior justifies the exponential decay law followed by the iodine entering the containment atmosphere (Ref. 1-9). In addition to this surface effect, steam condensation promotes the mass transfer of iodine and leaches out iodine on coated surfaces and concrete. The analyses of the last two effects is complex and still under consideration.

The above-mentioned processes refer to molecular iodine in the gaseous phase. For isotope-containing particles suspended in the containment atmosphere (aerosol), its deposition on the containment surfaces can presently be analyzed in two ways:

1. Converted HAA-3 Computer Program. The agglomeration, settling, and attachment of a log normal distribution of aerosol particles can be followed with the aid of the HAA-3 computer program. However, there are indications that the distribution of aerosol particles is not a single log normal distribution but a complex one. For instance, iodine in the containment atmosphere originates from direct iodine release and from tellurium radioactive decaying chains which follow different releases from the reactor core.

2. Simple Models. Since gravitational settling dominates the isotope deposition process over the attachment to containment walls (Ref. 1-1) and the description of the aerosol distribution is quite imprecise, the use a priori of simple models, as described in Ref. 1-10, is justified. In fact the model reported in Ref. 1-1 for gravitational settling corresponds to noninteracting iodine-containing particles in a stirred medium, simulating a homogeneously mixed containment atmosphere. The use of simple models rather than the elaborated HAA-3 computer program allows the further incorporation of these simple models into the CNTB computer program.

1.4. COMPUTER CODE DEVELOPMENT

Development and updating of computer codes for safety and reliability analysis is pursued on a continuing basis. The work includes development of new codes and modifications to improve modeling based on the recommendations of the AIPA study, NRL, national laboratories, and others, as well as documentation and users descriptions of present code versions.

In FY-78 the main task is to develop a method for analyzing a hypothetical design basis depressurization accident (DBDA) with a steam generator steam leak by modifying the computer code RATSAM. This quarter concluded the selection of analytical models for the primary and secondary sides.

1.4.1. Analytical Models

The thermodynamic behavior of the HTGR primary coolant system during a depressurization accident can be predicted by the RATSAM computer program (Ref. 1-11). Since this code can calculate the transient behavior of pressure, temperature, and flow throughout the primary coolant system for a wide variety of accidents, it was selected for this analysis. However, RATSAM does not presently model the secondary side steam/water system.

Therefore, the FLASH/GA computer program (Ref. 1-12) will be used to analyze the secondary coolant transient thermal-hydraulic behavior during postulated transients. This code is a modified version of the original FLASH code which was designed to analyze pressurized water reactor hydrodynamic transients (Ref. 1-13).

Analysis of an HTGR DBDA with steam leak requires the prediction of the transient hydraulic behavior within both the primary and secondary coolants. The combination of the RATSAM and FLASH codes into one comprehensive code that predicts the interrelated effects of coolant thermal-hydraulics, system heat transfer, and system component interactions (e.g., isolation and draining of the leaking steam generator loop) will provide the necessary tool needed to analyze steam generator inleakage during a DBDA.

1.5. NSS SYSTEMS ANALYSIS

The scope of the HTGR systems analysis is to support the safety evaluation of the gas-cooled reactor generic technology program in the areas of thermal, fluid flow, acoustic, and dynamic analysis. In FY-78 this task includes primary loop natural convection analysis during loop shutdown and acoustics and vibrational analysis related to establishing safety and design margins for primary loop components.

1.5.1. Acoustic and Vibrational Analysis

In this quarter the major portion of Phase II of the program to provide a statistical basis for acoustic vibration design criteria was completed. Probability-of-exceedance calculation methods for individual components have been extended to address a collection of similar components, such as an array of thermal barrier assemblies. The new analysis dispenses with the unacceptable assumption that exceedance events (e.g., actual stress exceeding calculated stress) which occur at various components are statistically independent events.

1.6. NSS SYSTEMS ANALYSIS METHODS DEVELOPMENT

Development and updating of HTGR systems analysis computer codes are performed in support of the generic effort in the thermal and fluid mechanics and dynamics area, including general thermal and fluid flow codes for isothermal and nonisothermal, laminar, and turbulent flow applications. The main activity in FY-78 is to produce turbulent flow code capability to determine local heat transfer due to hot streaks.

The general purpose thermal analysis code TAC2D, which is used in thermal analysis of safety-related components, was improved by adding on-line plot capabilities. This allows the user to obtain an immediate visual picture of the thermal behavior of the component being analyzed.

1.6.1. Turbulent Flow Code Development

The effort toward developing turbulent flow computer program capabilities was firmly established in the second quarter. The search for an existing outside computer program was completed. Part of this search included a comparison of the finite difference approach and the finite element approach for turbulent flow analysis. A finite difference computer program developed by Scientific Research Associates of Glastonbury, Connecticut, was selected. This computer program best satisfies the established criteria for gas-cooled reactor turbulent flow analyses. The program was originally developed with government funding and can be obtained with some modifications. A purchase order for the modified computer program has been initiated.

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2. STRUCTURAL ENGINEERING 189a NO. 00791; PROGRAM AREA 3

2.1. PRESTRESSED CONCRETE REACTOR VESSEL (PCRV)

2.1.1. Design

2.1.1.1. Load Monitor System

A study of load monitor systems to determine circumferential prestressing loads was initiated. The circumferential prestressing system is based on the use of bands of high-strength, low-relaxation steel strands wound under tension around the PCRV. The prestress in the strands, which relaxes with time, must provide the pressure-resisting forces in the PCRV throughout the design life of the vessel. The circumferential prestressing system load monitors installed at representative locations on the PCRV should be capable of indicating the prestress level in the PCRV at any time during the life of the vessel. The load monitors ensure that the required prestressing is applied and maintained.

The initial phase of this study was centered on the selection of a load monitor system. This phase was concluded with the selection of a radial load monitoring system with electrical load cell sensors. The electrical load cell sensors are based on the resistance measurement of a Wheatstone bridge. Each load monitor consists of four load cells. This concept offers redundancy in that the prestressing loads can be monitored continuously with one or more nonfunctioning load cells. Major design efforts are directed in devising a feasible scheme for removal of a load cell for repair or replacement. Other design considerations include:

1. Determining the radius of curvature of the load shoe such that acceptable accuracy of the radial load monitored can be obtained.

2. Accounting for nonuniform load distribution on the load shoe during the wire-winding process.
3. Limiting the transverse pressure on the strands to the allowable value so that the ultimate strength of the strand is not impaired.

2.1.2. Design Verification and Support

2.1.2.1. Qualification Tests of 11-MN (2500-kip) Tendons for Linear Prestressing System

The test analysis report on the 11-MN (2500 kip) tendon qualification tests was prepared and is being reviewed. The final test analysis report should be available in the early part of the next quarter.

The tendons consist of sixty 12.7-mm (0.5-in.) strands, designed and tested for GA by VSL Corporation of Los Gatos, California in August 1977. One 12.2-m (40-ft) long full-capacity straight tendon was tested to 103.5% of its specified minimum ultimate tensile strength and 2% elongation without any strand or wire breaks and without damage to the anchorage components, satisfying the requirements for static tensile testing specified in the ASME Boiler and Pressure Vessel Code, Section III, Division 2. Results of this static test and of dynamic tensile tests conducted by VSL in 1968 and 1969 on 12-strand tendons of similar design and materials indicate that the VSL tendon system fully complies with the ASME code and thus is considered suitable for PCRV construction.

2.1.2.2. Relaxation Tests of 12.7-mm (0.5-in.) Stabilized Strand

Relaxation tests of 12.7-mm (0.5-in.) stabilized strand for the PCRV linear prestressing and circumferential prestressing systems were completed. The tests were started in August 1977 and were conducted on four 27.5-m (90-ft) long tendons, each consisting of seven 12.7-mm (0.5-in.) strands and VSL E5-7 anchor heads. The tendons were initially tensioned to 70%

of their specified minimum ultimate tensile strength. Two tendons were initially tested at 70°C (158°F), one at 50°C (123°F), and one at 34°C (93°F). All four tendons were later heated to 71°C (169°F) and then to 93°C (200°F) and finally were cooled to ambient temperature.

Tendon liftoff tests were conducted which indicated that the zero load reading on the "Sigma" load cells had shifted considerably in the 6 months that the tendons were under tension, resulting in unusually high apparent relaxation losses near the end of the test period. Adjustment of tendon relaxation data to reflect the load cell zero shift is under way.

Examination of the tendons after the liftoff tests indicated no evidence of corrosion. Prior to initial tensioning, two of the tendons were covered with Visconorust 2889 grease and two with a thin film of CERAC SP-115 "Agua-Foil" dry wax. Visconorust is a permanent corrosion prevention compound extensively used with unbonded tendon systems, whereas CERAC SP-115 is classified as a temporary corrosion prevention coating. The four tendons were retensioned without load cells for further corrosion monitoring tests.

A report on the relaxation tests will be prepared next quarter.

2.2. LINERS

2.2.1. Design

A preliminary draft of a report which documents the leak tightness design for the HTGR liner has been completed. The report contains a discussion of the applicable codes and regulations, material and fabrication examination requirements, and preoperational leak testing. Corrosion and fatigue are discussed as potential crack or flaw initiation mechanisms. The liner material is shown to be able to arrest the growth of postulated flaws. Operational experience of the Fort St. Vrain and European PCRV liners is also discussed.

2.2.2. Design Verification and Support

2.2.2.1. Shear/Fatigue Tests of Liner Cooling Tubes and Anchor Studs

A cooling tube material availability problem has necessitated the purchase of materials of higher grade classification (and yield strength) than specified for the shear/fatigue tests. For the round tubes SA-106 Grade B was substituted for the specified Grade A. Low carbon steel A-500A square tubing was obtained in place of the specified SA-106 or SA-53 Type E or S. The substituted materials have been annealed to reduce their yield strengths to the levels of the specifications. This has caused a delay in the test starting date from March to May 1978.

A contract has been let for concrete placement. A series of batch tests have begun.

2.3. THERMAL BARRIER

2.3.1. Design

2.3.1.1. Lower Core Outlet Temperature Study

The overall objective of this study is to determine the potential benefits of lowering the core outlet plenum temperatures for the steam cycle (SC) HTGR. Benefits such as cost reduction by material and component design changes, as well as the improvement of design margins, are to be considered. The evaluation is to be conducted for a reduction of up to 110°C (200°F) in gas temperature in 28°C (50°F) decrements.

The Thermal Barrier Branch is supplying input to three major sections of the study report. The first section contains an evaluation of the present design feasibility including identification of design margins and programs (under way or proposed) to confirm feasibility. That task was completed in this report period. It was concluded that for the Class B

thermal barrier there is a positive margin. However, this conclusion is based on Hastelloy-X material properties which have not been affected by irradiation or corrosion. The ceramic portion of the Class C thermal barrier was identified as having a negative margin and, therefore, requiring redesign.

Work has begun on the remaining two report sections which are: (1) the general accrued benefits of lower component temperatures in the SC-HTGR, and (2) means of achieving greater margins between actual component temperatures and the component limits in the SC-HTGR.

2.3.1.2. Design Development Advancement

2.3.1.2.1. Alternate Material Candidate Evaluation. Two alternate Class B metallics have now been reviewed: Incoloy 800H and Hastelloy-XR. The work done on Incoloy 800H concludes that no consistent information could be found demonstrating that this alloy exhibits greater resistance than Hastelloy-X to embrittlement due to thermal neutron exposure. The data base for Hastelloy-XR was found insufficient for quantifying the radiation effect, although Hastelloy-XR does appear to be more radiation resistant than Hastelloy-X.

2.3.1.2.2. Insulation Blanket Assembly Requirement Assessment. Fibrous insulation procurement specifications were updated to reflect relaxations in thickness and density tolerances arising from contacts with insulation suppliers and the production insulation evaluations discussed in Section 2.3.1.3.

2.3.1.2.3. Incorporation of Power Plant Experience in Design. The objective of this work is to assure incorporation of nuclear power plant experience (including Fort St. Vrain) in the design of thermal barrier components. As part of an overall study of reactor experience incorporation into HTGR design, a detailed comparison of the current design with the

Fort St. Vrain design is under way and is now about 95% complete. To date, this comparison confirms that maximum use of Fort St. Vrain experience has gone into the current thermal barrier design.

2.3.2. Design Verification and Support

2.3.2.1. Load Bearing Ceramic Studies

A meeting was held between Southern Research Institute (SoRI) and GA to resolve outstanding technical and contractual issues which have been adversely affecting progress on the subject test subcontract for the last 6 months. An agreement was reached between SoRI and GA which stipulates an amendment to the existing purchase order whereby additional costs will be accommodated as had been previously agreed to by GA. However, in the case of machining costs, the contract made accommodation for adjustments consistent with expenditures incurred over and above those allowed for in the original purchase order. The amended purchase order will eliminate any further GA machining cost liabilities.

The revised Issue D of the test specification (Document No. 903130), which reflects all current requirements of the ceramic test program, was reviewed in detail with SoRI. No unresolved technical issues remain from this review, and GA will release Issue D concurrent with the amended purchase order. The following key dates were identified:

1. Completion of all elemental and full-size ceramic pad tests expected by May 15, 1978.
2. Completion of all creep specimen tests expected by February 1, 1979.

2.3.2.2. Chatter Tests

The GA cognizant engineer visited the Boeing Engineering and Construction plant to perform detailed evaluation of the test data. The

conclusion reached was that Phase I test 1 data were unacceptable but that test 2 data could be considered acceptable provided a valid repeat of test 1 was performed. A meeting was held between Boeing and General Atomic to negotiate mutually acceptable terms for the rerun of test 1. These goals were essentially achieved with some minor technical items being resolved subsequent to the meeting. Briefly, Boeing agreed to repeat test 1, with some modified test procedures, at no cost to General Atomic. General Atomic has agreed to accept the test data from the previously conducted test 2.

Pursuant to the subject meeting, Boeing and General Atomic have been exchanging updated test procedure and test specification documents. The repeat of test 1 is expected to be complete by the end of the next quarter.

2.3.2.3. Evaluation of Low-Density Saffil Alumina HT Insulation Blanket

GA received six low-density Saffil (LDS) blankets, manufactured by Imperial Chemical Industries (ICI), Runcorn, England, in October 1977. These were then submitted to an extensive testing program to determine whether the thickness and load variations in the production LDS blankets would result in compressive loads higher than 6.9 kPa (1.0 psi) on the cover plates of a typical Class B thermal barrier, consistent with present design assumptions. This testing program has just been completed and the detailed test data analysis report has been issued. The tests showed the following:

1. As could be expected, the nonuniformity of thickness and compressive load due to surface and volume irregularity encountered in most individual LDS blankets was attenuated when several blankets were combined with standard Kaowool to form a typical composite Class B thermal barrier fibrous insulation assembly.
2. The essential requirement of a moderate [≤ 6.9 kPa (1.0 psi)] and uniform compressive load was found to be met by all six tested composites, covering the entire spectrum of production LDS qualities.

On this basis, GA considers that production LDS as manufactured by ICI is acceptable for the Class B thermal barrier as regards the loads applied by the fibrous insulation on the cover plates. From GA's observations, once the composite insulation was compressed to its installed thickness, there remained no apparent gross defects which would allow helium to flow at the fibrous insulation/seal sheet interface and through the thickness of the insulation and thus jeopardize the efficiency of the thermal barrier. However, a conservative approach will be taken whereby it will be preferred to avoid using those blankets or portions of blankets containing large ridges, pleats, or whorls.

2.3.2.4. Proposals for U. S. DOE/FRG Umbrella Agreement

A proposal work statement was drafted for thermal barrier studies under the DOE/FRG cooperative agreement. The statement will be transmitted to the KFA in Jülich for review and will be discussed at meetings proposed in Jülich early in the next quarter.

The general program objective is to develop thermal barrier component technology for long-term application in HTGR environments at normal design condition temperatures up to about 1000°C (1830°F). The proposed program draft consists of the following seven subtasks:

1. Perform a comprehensive generic evaluation of candidate metallic materials for thermal barrier application, primarily in the intermediate-temperature [400° to 700°C (750° to 1290°F)] and high-temperature [700° to 1000°C (1290° to 1830°F)] ranges of the HTGR environment.
2. In the manner described under Subtask 1, perform an analogous evaluation of candidate ceramic materials for thermal barrier components operating at normal design condition temperatures up to about 1000°C (1830°F).

3. Perform joint U. S./FRG development of design criteria for metallic thermal barrier materials.
4. Perform joint U. S./FRG development of design criteria for ceramic thermal barrier materials, both load-bearing and non-load-bearing.
5. Perform conceptual and preliminary design development of alternate high-temperature hot duct thermal barrier configurations for operation in the 800° to 1000°C (1470° to 1830°F) temperature range.
6. In a manner analogous to Subtask 5, develop alternate metallic and ceramic conceptual and preliminary thermal barrier designs for general application on reactor cavity floors, walls, and ceilings.
7. Perform detailed planning of a comprehensive vibration assessment program for the purpose of investigating and qualifying proposed thermal barrier designs for the anticipated HTGR acoustic environments. Conduct tests consistent with such a development plan.

2.3.2.5. Vibration Assessment Studies

Preliminary planning of testing and analytical studies was performed. Detailed analysis and planning for the next test phase will continue through the remainder of this fiscal year.

2.3.2.6. Long-Term Resiliency Test

The function of the thermal barrier is to protect the PCRV liner surface and adjacent concrete from overheating due to exposure to high-temperature helium. This protection is provided by layers of fibrous

insulation compressed against the liner by cover plates and attachment fixtures. The integrity of the thermal barrier depends on how well the fibrous layers retain their resiliency under high operating temperatures. Resiliency prevents formation of gaps which would permit increased hot gas permeation within the thermal barrier.

The general test objective is to establish resiliency characteristics of candidate Class B HTGR insulating materials over extended time periods at operating temperatures and temperature gradients. Specific objectives are:

1. Compare and establish the long-term resiliency of various Class B blanket configurations employing high- and low-density Saffil Alumina HT, Quartz et Silice, Kaowool XS, Kaowool, and Fiberfrax W.
2. Determine if Kaowool can be used for long-term service at hot surface temperatures up to about 650°C (1200°F).

This test program explores material properties for durations up to 20,000 h in increments of about 5,000 h. Testing consists of simultaneously submitting 56 compressed insulation specimens to realistic Class B temperatures and temperature gradients, measuring compressive loads before and after the test, and determining spring back after removal of restraints. In addition, compressive loads are monitored on six selected specimens intermittently during the test. Square specimens, 25 cm x 25 cm (10 in. x 10 in.), are used for all tests. All testing is performed in a nitrogen purge at 1 atm pressure. Test specimen identification is clearly traceable throughout testing and test report preparation.

The test chamber is a steel box 107 cm (42 in.) wide by 99 cm (39 in.) deep by 196 cm (77 in.) high. The chamber employs interior electric heaters and an external water cooling system. Test specimens are bolted to the chamber interior surfaces. A schematic of the test chamber is shown in Fig. 2-1. Test specimens without in-service load monitoring are shown in Fig. 2-2, and specimens with load monitoring are shown in Fig. 2-3.

Fig. 2-1. Test chamber configuration for long-term resiliency test

Fig. 2-2. Resiliency test specimen assembly configuration

Fig. 2-3. Configuration for in-service load monitoring

Each specimen employs a square assembly of fibrous insulation composed of several individual insulation blankets. The fibrous insulation will be compressed between metal plates as it will be in the HTGR thermal barrier. A 10-mm (0.375-in.) thick plate and a 0.8-mm (0.31-in.) thick Hastelloy-X sheet constitute the top cover of each specimen, thus modeling the reactor thermal barrier cover plate and seal sheet. A 10-mm (0.375-in.) thick plate on the bottom of each specimen assembly and the adjacent test chamber steel wall simulates the reactor liner. Test specimen operating temperatures are measured with thermocouples attached to the seal sheet and back plate on the side in contact with the fibrous insulation.

The test conditions are as follows:

Hot surface temperature: ~650°, 815°, 980°C (1200°, 1500°, 1800°F)

Cold surface temperature: about 66°C (150°F)

Slow nitrogen purge: autoclave atmosphere is unpurified
nitrogen-air mixture

At present, the test rig is in continuous operation after shutdowns for data collection and repairs. About 6,000 h of testing have been accumulated to date.

2.3.2.7. Replaceable Thermal Barrier Study

GA approval was given for publishing the final study report. The report will be transmitted to DOE early in the next quarter.

2.4. REACTOR INTERNALS

2.4.1. Design

2.4.1.1. Revisions to ASME Boiler and Pressure Vessel Code

Graphite structural criteria are being written into the ASME Boiler and Pressure Vessel Code, Division 2, Subsection CE. Structural criteria studies are being carried out as a part of this program for inclusion in the Code. Revisions to the initial draft of Section CE have been initiated in preparation for a preliminary review submittal to the Code Committee during the fourth quarter of FY-78.

2.4.1.2. Core Outlet Temperature and Core Support Block Oxidation Parametric Study

A parametric study will be performed to determine the following:

1. The allowable oxidation rate for the maximum core outlet temperature.
2. For any given oxidation rate, the corresponding allowable core outlet temperature.

A three-dimensional stress analysis model is being prepared for a 30° sector of the core support block for use in the study to determine the allowable depth of attack for the proposed design. This is basic information needed to perform the remainder of the study.

2.4.1.3. Oxidation-Resistant Core Support Structure Study

A study will be performed to determine the most advantageous means of providing an oxidation-resistant core support structure. The study will investigate a number of oxidation improvement alternates, including the addition of liners to flow ducts, the use of a composite block design of a different material, and the possibility of using a completely new graphite.

The study has been started with investigation of the inclusion of liners on the wetted flow surfaces of the core support block, with the liners being made from a different grade of material that is less susceptible to oxidation attack.

2.4.2. Design Verification and Support

2.4.2.1. Graphite Thermal Stress Limit and Fatigue Tests

Specimens of core support and permanent side reflector graphites will be tested under conditions inducing large thermal gradients to develop design allowable values for secondary thermal stresses in both single-cycle and fatigue conditions. The design function of the core support and permanent side reflector requires that these components maintain a high performance capability throughout the life of the reactor. These thermal stress limit and fatigue tests of the various graphites used will enable the development of analytical methods and failure criteria which will result in a safe, reliable design.

Coupon evaluation specimens of the core support graphite materials will be tested to determine the basic material properties, e.g., tensile and compressive strengths, thermal expansivity, density, etc. Specimens will then be tested to determine thermal gradient versus input power in an induction heating apparatus. Single-cycle thermal stress failure tests will then be performed to ascertain the gradient and associated stress which causes rupture of the specimens. Fatigue cycles will then be applied, producing thermal stresses between 80% and 90% of the single-cycle failure stress. Graphites PGX, 2020, and ATJ will be tested, in that order.

Coupon evaluation testing of both PGX and 2020 material and low-power (50 KVA) single-cycle thermal stress testing of PGX have been completed. The high-power (500 KVA) testing of PGX and low-power testing of 2020 have been started. Figures 2-4 and 2-5 are typical plots from the 2020

MATERIAL: 2020
 TEMPERATURE: 538°C (1000°F)
 LOADING DIRECTION: ACROSS GRAIN
 SPECIMEN GAGE SECTION: 0.220 X 0.875

ULTIMATE

1000 PSI (6.89×10^6 PA)
 TENSILE STRESS

$$\sigma = 1000 (4.25) = 4250 \text{ PSI } (29.29 \times 10^6 \text{ PA})$$

$$\epsilon = (0.0005) 6.65 = 0.0033 \text{ IN./IN. (MM/MM)}$$

$$E = \frac{1000}{(0.0005) 1.35} = 1.48 \times 10^6 \text{ PSI } (10.2 \times 10^9 \text{ PA})$$

0.0005 IN./IN. (MM/MM)

Fig. 2-4. Stress/strain curve - tension

Fig. 2-5. Stress/strain curve - compression

coupon evaluation tests, showing stress/strain curves for both tension and compression. Figures 2-6 and 2-7 are typical plots from the low-power single-cycle thermal stress tests for PGX and 2020, respectively.

The final phase of PGX testing is the application of high-power (500 KVA) thermal fatigue cycling, which will be completed during the fourth quarter of FY-78. The high- and low-power tests on 2020 will be completed during the third quarter of FY-78. The preparation of specimens and the initiation of coupon evaluation testing of ATJ material will occur during the third quarter of FY-78.

2.4.2.2. Thermal Mixing Tests

Tests are being conducted on a range of flow channel configurations in the bottom reflector blocks and core support blocks with the objective of improving mixing of the coolant streams from the different columns of a fuel region, at the least possible penalty in increased pressure drop. Better mixing is needed to meet specified measurement accuracy of outlet gas mean temperature for rodded regions. Structural benefits will also accrue from reduced column hot streak effects on core support posts and the lower plenum thermal barrier.

Figure 2-8 shows a typical core support block configuration with the miter angle defined as θ and the overall block height as "A". The thermocouple sleeve is shown in phantom for reference.

Four core support block configurations were selected for a series of preliminary screening tests which were performed on half-scale models using hot air. Analytical evaluation of the results obtained from these tests has been completed. Mixing results were determined from the variation of temperatures indicated by a grid of thermocouples across the outlet end of the core support block model. The mixing data were then correlated with expected costs associated with pressure drop and with the effect of block height on core cavity height (hence PCRV and containment building costs, etc.).

Fig. 2-6. PGX thermal stress results, sheet 1

Fig. 2-6. PGX thermal stress results, sheet 2

Fig. 2-7. 2020 thermal stress results, sheet 1

Fig. 2-7. 2020 thermal stress results, sheet 2

Fig. 2-8. Typical core support block configuration

The configuration which provided the optimum apparent cost/benefit characteristic was a 1.35-m (53-in.) high core support block with a 52° miter angle. A 1.04-m (41-in.) high core support block with a 70° miter angle was shown to have only slightly greater cost effect, and to provide slightly better mixing.

Several variations of both the 52° and the 70° core support block designs are being considered for the final phase of this test program. The last phase will be completed during the early part of the fourth quarter of FY-78 with the final results to be reported by the end of FY-78.

2.5. STRUCTURAL MECHANICS

2.5.1. Design Verification and Support

The overall objective of this task is to develop, verify, and maintain state-of-the-art analysis techniques in the areas of structural mechanics, structural dynamics, and seismic analysis. In FY-78 the activities under this task are divided into two work areas: core seismic program and general methods development.

2.5.1.1. Core Seismic Program

The core seismic program is an ongoing effort which aims to develop the necessary analytical and test technology to evaluate the seismic adequacy of the HTGR core and core support structures. This program interfaces with the Fuel Element Design task, which covers seismic strength tests and stress analysis. These activities are reported elsewhere.

This quarter, an effort was initiated to update the current core seismic plan (Ref. 2-1) for seismic qualification of the core and core support system. The plan will contain details of further methods development, analysis, and testing needed to conclude the seismic verification issue and satisfy licensing needs.

2.5.1.1.1. Model Testing. This subtask deals with scaled model tests designed to provide basic characteristic data on individual core element behavior and to give information on the interaction of groups of elements including two- and three-dimensional core arrays. The test data are used for correlation and verification of the analytical codes. These tests have been described in previous progress reports and their preparation was completed prior to FY-78. The test reports are now in various stages of preparation and review and will be issued during the next quarter.

2.5.1.1.2. Analytical Development. This subtask deals with the development of all core seismic codes and the correlation of these codes with test results. The codes are CRUNCH-1D, CRUNCH-2D, COCO, and the multicolumn code MCOCO. The mathematical and modeling methods in these codes have been described in previous quarterly progress reports.

The documentation of CRUNCH-2D has completed review and is in the final stages of revision and approval for issue. Documentation of MCOCO was completed in draft form, reviewed, and submitted for final report preparation.

A plan for the investigation of a reduced array MCOCO model was developed. This includes reducing the number of blocks in a column model and the total number of columns representing a strip across the core. The feasibility of this technique was demonstrated by the CRUNCH-1D and COCO codes. By reducing the 37-element CRUNCH-1D strip model to 18 elements, the results were shown to correlate within the sensitivity of the code. This reduction has the potential of reducing the MCOCO run time by 40% to 50%. The results from a single-column COCO model with 13 elements showed that a 13-block column can possibly be reduced to as few as 5 blocks. However, the single-column COCO model has a high degree of numerical sensitivity and, consequently, the study must be confirmed using MCOCO. A MCOCO model with the reduced number of elements in a column is currently being developed.

The computer program MCOCO has been modified to model the additional restraint provided by the reflector ring when its blocks are in contact around the circumference. Two types of rotational constraints for the reflector elements are being considered. A full-scale, eight-column model is being developed in order to evaluate the reflector link mechanism.

2.5.1.1.3. Core Seismic Methods Verification. This subtask includes the work performed in correlating the analysis and test data, thereby attempting to verify the analytical models. The results of this correlation and verification process will be reported in a Core Seismic Methods Verification Report, a status report to be published in June 1978. The report, which will give the status of the core seismic program to date, also contains summary documentation of all analytical development and testing in addition to recommendations for further studies in areas where needed. The Core Seismic Methods Verification Report is currently being reviewed.

2.5.1.2. General Methods Development

Methods development for the analysis of gas-cooled reactor components, including the PCRV, core elements, and core support structures, as well as soil/structural interaction, has been a major effort in prior years. Presently, computer code updates are performed to keep abreast of new techniques in this area, and modifications are made to support NRC licensing requirements and to implement the most cost effective methods for analysis, as well as code documentation and maintenance.

2.5.1.2.1. Cathode Ray Tube (CRT) Hardware and Software Development. This subtask deals with the development of software for interactive CRT demand processing terminals to improve data preparation and displays of results. An extensive study of the available CRT equipment, as well as the requirements, was conducted in making the selection of equipment to be purchased. Four Lear Siegler ADM3A alphanumeric computer terminals, one Tektronix 4014-1 graphics terminal, and one Tektronix hard copy unit were ordered. The four alphanumeric terminals were received during the current reporting period. The remaining equipment is scheduled to arrive in May 1978.

A seminar was given to acquaint personnel with the terminal and demand processing. The topics covered included an introduction to the the terminal, demand versus batch processing, how to log on and off the computer, a review of Univac control statements, and submittal of batch jobs from the terminal. The seminar also included a demonstration where participants gained experience entering data and executing programs from the terminal.

REFERENCE

- 2-1. Shatoff, H. D., D. A. Wesley, and B. E. Olsen, "HTGR Core Seismic Verification Plan," General Atomic Report GA-A13934, June 1976.

3. HEAT EXCHANGER EQUIPMENT
189a NO. 00793; PROGRAM AREA 4

3.1. DESIGN

3.1.1. Core Auxiliary Heat Exchanger (CAHE) Design and Analysis

The CAHE is part of the core auxiliary cooling system (CACS) and provides auxiliary primary coolant system heat removal capability for core cooling in the event that the main cooling loops are shutdown or unavailable. The CAHE must provide the required cooling capability for core cool-down under both pressurized and design basis depressurization accident (DBDA) conditions. The CAHE normally operates in a "standby" mode, during which a minimal flow of water is maintained through the unit. This ensures that the CAHE will be in a ready-standby mode at CACS startup.

The CAHE design concept is based upon a bayonet tube heat exchanger arrangement. Water flows inside the tubes and helium flows over the outside of the tubes, parallel to the tubes and counterflow to the water. Piping connections are provided at the bottom of the heat exchanger to convey cooling water to and from the unit. The bayonet heat transfer tubes (pressure boundary) are joined to the primary coolant boundary tubesheet at the bottom of the CACS cavity in the PCR. Internally, water is conveyed to the upper end of the bayonet tubes where it reverses direction and picks up heat enroute to the water outlet plenum below the tubesheet.

This bayonet tube configuration for the CAHE was conceived during the development of the 3360-MW(t) Lead Plant. The work conducted during this reporting period was development of a reduced size conceptual CAHE design for a smaller plant size.

The 2240-MW(t) plant utilizes three CACS loops as did the Lead Plant. Initially a single common CAHE size and design were considered for use in either plant size; however, further evaluation indicated that a CAHE resized for the 2240-MW(t) plant was preferred, as this approach permitted a more optimized CACS design and more closely duplicated the CAHE operating conditions for each of the two plant sizes.

A conceptual general arrangement drawing (Fig. 3-1) was prepared for the CAHE for the 2240-MW(t) plant, completing the first quarter FY-78 milestone. The same CAHE design can be used in any of the three 2240-MW(t) alternate plant configurations under consideration. This conceptual general arrangement drawing was utilized to set the CAHE size and interfaces within the PCR.V. It was also used for cost estimating purposes.

The conceptual configuration incorporates 360 38.1 mm (1.5 in.) O.D. heat transfer tubes and a flat tubesheet. Tubing in-service inspection (ISI) capability is obtained by removal of the inner tubes for access to the pressure boundary tubing. Work will continue to further develop the design of the bayonet tube CAHE leading to completion of a conceptual design by the end of the fiscal year.

3.1.2. Steam Generator Design and Analysis

The main support flange (MSF) of the steam generator (SG) is the primary load-carrying member for the SG structure. It mates with and is supported by a similar support flange which extends via a thermal sleeve from the cavity liner that supports and locates the SG within the PCR.V cavity. The SG-MSF is subject to dead weight and seismic loads and also loads induced by thermal gradients resulting from differing primary coolant and feedwater temperatures.

The current SG-MSF concept is a bolted design. It incorporates a spiral wound "Flexitallic" type gasket between the SG-MSF and the liner flange to prevent overheating of the SG-MSF. The gasket prevents leakage

- NOTES:
- 1 THESE DIMS INCLUDE APPROX 100 FOR ATTACHMENT FEATURES AND ASSOCIATED TOLERANCES
 - 2 ALL PCRV, THERMAL BARRIER AND LINER DIMS ARE FOR REFERENCE ONLY
 - 3 THIS DWG MAY ALSO BE USED ONLY FOR COSTING OF CONCEPTS 'A' AND 'B'

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Fig. 3-1. Conceptual general arrangement drawing for CAHE

of higher temperature helium from the lower plenum of the SG cavity into the annular region at the helical bundle gas outlet located above the MSF. This concept also relies on a cool helium purge, or "buffer flow" of helium, from the discharge of the main circulator to cool the steam generator and liner flanges. The purge or buffer flow rate provides a means of monitoring the integrity of the seal between the two mating flanges.

Concerns over the design of seal and purge systems as conceived have centered on the following:

1. The adequacy of pressure taps located at the flanges to monitor seal leakage.
2. The reduction in the amount of helium buffer flow during CACS operation due to the lower helium pumping head available from the auxiliary circulator. There may be insufficient buffer flow to cool the flanges and suppress the convection of hot helium from the lower hot duct into the flange area.
3. The inability to accurately predict seal leakage and the resulting purge system performance uncertainties, and the effect of flow variations on core power.
4. The cost of providing the flange purge system and the effect of the system on plant performance.

Because of these concerns a study of alternate MSF design concepts was conducted. Three alternates were chosen and layouts of these concepts were completed, satisfying the second quarter FY-78 milestone. These layouts are shown in Fig. 3-2.

The conclusions resulting from the study are:

1. The mechanical metallic ring seal support flange concept has historically been poor as a zero or low-leakage static seal

device and may not provide an effective leak-tight seal between the interfacing surfaces of mating support flanges. The ineffectiveness of the seal is compounded by its large diametral size and the surface finish and waviness of the flange faces. Consequently, the need for a purge gas and the problems associated with providing it, as identified for the current configuration, are characteristic of this type of seal concept.

2. Retaining balls, cams, and close-fitting bolts extending through the flanges require close tolerance control to ensure even distribution of loads through all retaining devices. Steam generator removal after some period of service may be hampered or even possibly prevented due to self-welding of these close-tolerance devices with the surfaces they contact.
3. From the evaluation of the various concepts, it appears that a "welded" flange design has the most merit; it has no seals, provides zero leakage, requires no helium purge system, and has less overall cost. Disadvantages include the requirement for remote welding and inspection capability and difficulty of repair.

Alternates A and C of Fig. 3-2 represent examples of welded main support flange concepts. It has been recommended that evaluation of the welded flange concept be pursued further to investigate flange temperatures, reliability, and ease of steam generator installation, removal, and reinstallation.

3.2. DESIGN VERIFICATION AND SUPPORT (DV&S)

3.2.1. DV&S Plan

Development and preparation of an overall heat exchanger (steam generator and CAHE) DV&S plan was a task scheduled for the fourth quarter of the fiscal year. Effort was initiated on this task in the second

Fig. 3-2. Alternate MFS design concepts

quarter in order to provide earlier visibility of the program in support of the DOE and GCRA evaluation of an accelerated startup date for the 900-MW(t) steam cycle HTGR plant.

3.2.2. Tubing Bimetallic Weld Design Criteria

A weld between metals having different coefficients of thermal expansion will be subjected to discontinuity stresses when the metal temperature is changed. A preliminary study to determine the allowable stress limits at elevated temperatures for a bimetallic weld (BMW) between 2-1/4 Cr - 1 Mo steel and Alloy 800H in heat exchanger tubing has been completed. BMW stress limits are based on creep-fatigue and strain accumulation criteria from ASME Boiler and Pressure Vessel Code Case N47-11 (Code Case 1592-11). The allowable elastically calculated stress ranges set by creep-fatigue limits are presented in Fig. 3-3. The stress range limit set by strain accumulation is presented in Fig. 3-4. Table 3-1 presents equations for use with these figures. The design criteria presented account for the weekly power-following cycle and the base-loaded condition with monthly shutdowns, for both creep-fatigue and strain accumulation.

Several conclusions have resulted from this study:

1. The pressure stress appears to be the most critical in the BMW region as it affects both criteria given above. Also, it is not usually practical to change the tube wall thickness to reduce the pressure stress significantly to allow a higher mean wall temperature at the BMW.
2. The bending stress in the BMW is drastically affected by the location of the BMW. Careful selection of the location will therefore be necessary to make optimum use of the load-carrying capability of the tubular BMW.

Fig. 3-3. BMW creep-fatigue limit; allowable primary plus secondary stress ranges for tubular BMW between Alloy 800H and 2-1/4 Cr - 1 Mo

Fig. 3-4. BMW hoop ratcheting limit; allowable pressure stress range for tubular BMW between Alloy 800H and 2-1/4 Cr - 1 Mo

TABLE 3-1
EQUATIONS FOR APPLICATION TO BMW CRITERIA

Criteria	Loading	Direction ^(a)	
		Axial	Hoop
Strain accumulation	Pressure stress due to P_i internal pressure and P_o external pressure	Not used	$\frac{P_i(b^2 + a^2) - 2P_o b^2}{(b^2 - a^2)}$
Creep fatigue damage	Pressure stress due to P_i internal pressure and P_o external pressure	$\frac{P_i a^2 - P_o b^2}{(b^2 - a^2)}$	$\frac{P_i(b^2 + a^2) - 2P_o a^2}{(b^2 - a^2)}$
	Radial thermal gradient stress due to ΔT Where circumferential temperature variation is small use Where circumferential temperature variation is significant use	Hoop and axial stresses are the same magnitude $\frac{\Delta T E \alpha}{2(1 - \gamma) \log_e(b/a)} \left[1 - \frac{2b^2}{(b^2 - a^2)} \log_e \left(\frac{b}{a} \right) \right]$ $\frac{\Delta T E \alpha}{2(1 - \gamma)}$ for both hoop and axial	
	Bending stress	σ_b	Ignore
	Summation	Compare with axial curve of Fig. 3-3	Compare with hoop curve of Fig. 3-3

- (a) a = internal radius,
 b = external radius,
 E = Young's modulus at operating temperature,
 α = mean coefficient of thermal expansion at operating temperature,
 γ = Poisson's ratio at operating temperature.

3. The stress due to the radial thermal gradient can be reduced by reducing the heat flux at the BMW. This can be accomplished by locating the BMW in a quiescent (no gas flow) zone or by the use of local insulation. Reducing the heat flux has a dual benefit since in addition to reducing the radial thermal gradient stress, it also reduces the mean wall temperature.

3.2.3. Tubesheet Size Limitations

A parametric study of different superheater tubesheet configurations and various tubesheet diameters was conducted. An elastic analysis was performed on models of tubesheets and the cylindrical transition barrel section that connects the tubesheet to the penetration extensions for 500-, 750-, and 1000-MW(t) plant sizes, for a design pressure of 17.3 MPa (2515 psi) and a temperature of 510°C (950°F). It was found that within the range of tubesheet sizes analyzed, the tubesheet-barrel region was insensitive to the variation of diameters, whereas the ligament stresses at the junction of the perforated region and the solid rim were found to increase as the tubesheet size increased. It was also concluded that the slow depressurization transient was more severe than the reactor trip transient from either 100% or 25% feedwater flow, both of which had been previously evaluated as being more severe. The conclusions arrived at may not apply to other tubesheet sizes operating at different temperatures and pressures.

3.2.4. Harwell Heat Transfer and Fluid Flow Service (HTFS)

Membership in the Harwell Heat Transfer and Fluid Flow Service has benefited many aspects of the HTGR program. Over the past year the HTFS literature survey capability has been utilized and a useful heat exchanger analysis computer program has been obtained; continued use is being made of the many design reports issued by HTFS. In the coming 2 years GA anticipates obtaining a sophisticated and verified steam generator dynamic

stability analysis computer program and design reports dealing with heat exchanger fouling, finned tube heat transfer and pressure drop, a turbulent flow analysis computer program, and tube bundle vibration data. This information and data will be useful in the preliminary and final analysis phases of the CAHE and steam generator development.

4. MECHANICAL AND ELECTRICAL COMPONENTS 189a NO. 00799; PROGRAM AREA 5

4.1. MAIN HELIUM CIRCULATOR

4.1.1. General

The major milestones for the first and second quarters of FY-78 have been completed. The initial conceptual design with the materials list has been developed.

The design feasibility and configuration evaluation has been completed for the "Configuration Evaluation Report," (Ref. 4-1); Section 7.4 covers the design aspects and Section 8.0 the development.

4.1.2. Design

4.1.2.1. Conceptual Design

A series of design layouts of the possible basic electric-motor-driven circulator configurations has been developed. The layouts are shown in Figs. 4-1 through 4-4.

Concept "A" is shown in Fig. 4-1. In this design the compressor is driven by an external motor. The compressor bearing system is water lubricated and uses technology similar to that of the Fort St. Vrain helium circulator. It incorporates substantial fluid system simplification and improved reliability. The drive motor is a vertical, 4500-rpm, oil-lubricated, synchronous motor with a brushless exciter. The motor construction is similar to a forged rotor generator. In this configuration the circulator is placed in its own separate PCRV cavity rather than sharing a cavity with the steam generator. A high-pressure shaft seal is needed at the drive motor end of the circulator. This shaft seal must prevent or

4-2

Fig. 4-2. "A prime" circulator concept, upflow diffuser and steel closure plug

Fig. 4-3. "A prime" circulator concept, downflow diffuser and concrete closure plug

Fig. 4-4. Submerged electric motor drive circulator

control the leakage of bearing water [6.5 MPa (950 psi)] to the containment atmosphere. Circulator flow control is by means of variable speed, which is accomplished with a solid-state variable-frequency power supply to the motor.

The "A prime" circulator concept is shown in Figs. 4-2 and 4-3. The compressor and drive motor are similar to the concept "A" version; that is, the compressor runs on water bearings and is driven by an external electric motor. The major difference is that the circulator is in the same cavity as the steam generator. One version (Fig. 4-2) has an upflow diffuser combined with a steel closure plug and the other (Fig. 4-3) has a downflow diffuser and a concrete plug.

Figure 4-4 is a submerged motor drive. In this version the motor is placed in the high-pressure helium atmosphere and the output shaft of the motor is bolted directly to the compressor disc. The circulator motor could be supported by oil- or water-lubricated journal bearings. The submerged motor concept is a possible second choice. However, this concept has more complex development problems and high motor windage losses.

4.1.2.1.1. Thrust Bearings. Preliminary calculations of the thrust loads have been completed for a single-stage compressor. The results for 100% speed and full pressure are as follows:

Shaft ejection load	=	11,818 kg (26,000 lb)
Aerodynamic load	=	14,727 kg (32,400 lb)
Weight load	=	7,727 kg (17,000 lb)

With an upflow diffuser the resulting thrust is 10,636 kg (23,400 lb) down at 100% speed, full pressure; 4,091 kg (9,000 lb) up at zero speed, full pressure; and 7,727 kg (17,000 lb) down at depressurized condition.

The thrust bearing is of the shrouded step design similar to that used in the previous HTGR water bearing concepts. The thrust range indicated above is within the capability of a typical bidirectional water thrust bearing.

4.1.2.1.2. High-Pressure Water Seal. A vital component of the external electric drive circulator is the high-pressure water seal that separates the circulator bearing water system from the containment atmosphere. This seal must control or eliminate the leakage of 6.5 MPa (950) psi) water down the 139.7 mm (5.5 in.) diameter drive shaft. Calculations have shown that a stepped clearance seal would leak about $126 \times 10^{-5} \text{ m}^3/\text{s}$ (20 gpm); a single floating ring seal with tighter clearances would leak about $32 \times 10^{-5} \text{ m}^3/\text{s}$ (5 gpm). A mechanical face seal would eliminate the leakage altogether. Present mechanical seal technology is sufficient to meet GA requirements. The application is very similar to the reactor coolant pump shaft seals for a pressurized water reactor. The seal arrangement would be designed to allow in situ replacement.

4.1.2.1.3. Rotor Dynamics. A vendor study of the proposed drive motor has indicated the feasibility of obtaining first critical speeds above the maximum operating speed of 4500 rpm. This analysis has been confirmed by GA rotor dynamics computer codes.

4.1.2.2. Aerodynamic Design

4.1.2.2.1. Single-Stage Compressor Design for 4500 rpm. For maximum simplicity of machine layout, a single-stage axial compressor was used in the conceptual design of the main circulator for motor drive at 4500 rpm. At the HTGR main circulator design conditions shown in Table 4-1 and this operating speed, the specific speed is:

$$N_s = \frac{N\sqrt{Q_1}}{(\Delta H_{is})^{3/4}} = 256 \quad , \quad (4-1)$$

where N = rotational speed (rpm),
 Q_1 = compressor inlet volumetric flow (ft^3/s),
 ΔH_{is} = isentropic head rise in compressor (ft-lbf/lbm).

TABLE 4-1
DESIGN CONDITIONS FOR MAIN HELIUM CIRCULATOR, CONCEPT "A"

Helium mass flow rate, ^(a) kg/s (lb/s)	282 (621.9)
Helium inlet pressure, MPa (psi)	6.267 (909.1)
Helium exit pressure, MPa (psi)	6.376 (925)
Helium exit temperature, K (°F)	600 (620)

^(a) Includes 2% allowance for leakage.

This corresponds to the low end of the range of N_s for which the axial flow stage is appropriate. Consequently, the design is typified by a high tip diameter, low blade height, and reduced efficiency. However, the single-stage compressor does produce a fairly simple machine layout, as shown in Fig. 4-1.

It has been shown that for the specific speed indicated above, a compressor tip diameter of 1270 mm (50 in.) is optimum. Iteration for axial velocity with corresponding blade height showed that an optimum combination with axial velocity to blade speed ratio around 0.75 gave an axial velocity of around 165 to 180 m/s (550 to 600 ft/s). A parametric study of designs with axial velocities over a range between 150 and 200 m/s (550 and 700 ft/s) and tip diameters between 1140 and 1270 mm (45 and 50 in.) was carried out using an axial flow compressor design computer program. This showed that although best efficiency was obtained for an axial velocity of 167 m/s (550 ft/s), the centrifugal blade stress tended to be too high, and a better compromise was obtained with an axial velocity of 182 m/s (600 ft/s). The optimization study on the compressor stage still continues and will be completed by the next reporting period.

The resulting conceptual design for the single-stage machine has the design parameters listed in Table 4-2 and has an overall efficiency, including exit diffuser, of 74.9%.

4.1.2.2.2. Two-Stage Compressor Design for 3600 rpm. To determine what improvement in efficiency could be obtained at the expense of some mechanical complexity, studies of two-stage compressors for this application were carried out. The first study considered a compressor driven by a 3600 rpm motor.

At this rotational speed, the stage specific speed for a two-stage compressor under the conditions of Table 4-1 is 344. This is more nearly optimum for axial flow compressors. A preliminary sizing of the rotor dimensions

TABLE 4-2
MAIN HELIUM CIRCULATOR CONCEPTUAL DESIGN, CONCEPT "A"

Design Parameter	Single Stage, 4500 rpm	Two Stage, 3600 rpm	
		Stage 1	Stage 2
Helium axial velocity, m/s (ft/s)	182 (600)	111 (365)	111 (365)
Rotor tip diameter, mm (in.)	1270 (50)	1231 (48.5)	1230.6 (48.45)
Rotor hub diameter, mm (in.)	1118 (44)	914 (36.0)	914 (36.0)
Rotor			
Mean diameter inlet angle, degrees		61.19	61.18
Mean diameter exit angle, degrees		50.87	50.82
Hub diameter chord, mm (in.)	60.53 (2.383)	75.59 (2.976)	75.59 (2.976)
Hub diameter stagger angle, degrees	43.73	43.94	43.94
Tip diameter chord, mm (in.)	48.77 (1.920)	49.78 (1.960)	49.89 (1.964)
Tip diameter stagger angle, degrees	51.19	58.12	58.08
Stator			
Mean diameter inlet angle, degrees		30.51	30.53
Mean diameter exit angle, degrees		0	0
Hub diameter chord, mm (in.)	46.20 (1.819)	55.96 (2.203)	55.96 (2.203)
Hub diameter stagger angle, degrees	12.85	13.51	13.51
Tip diameter chord, mm (in.)	4612 (1.819)	55.96 (2.203)	55.96 (2.203)
Tip diameter stagger angle, degrees	11.59	10.73	10.74
Stresses			
Rotor blade centrifugal, MPa (psi)	70.93 (10,289)	74.35 (10,785)	73.96 (10,719)
Nominal blade bending, MPa (psi)	69.8 (10,000)	69.8 (10,000)	69.8 (10,000)
Performance			
Compressor efficiency, %	87.99	89.3	89.3
Overall efficiency, %	74.91	81.7	81.7
Power input, kW (hp)	7672 (10,987)	7484 (10,036)	7484 (10,036)

indicated an optimum hub diameter of 914.4 mm (36 in.) and an optimum axial velocity of 111 m/s (365 ft/s). To investigate the optimization more thoroughly and to resolve the difference between the two rough sizing methods, a parametric design study was carried out on the two-stage compressors, using a compressor design and analysis program. Hub diameters between 914 and 1219 mm (36 and 48 in.) and axial velocities between 111 and 198 m/s (365 and 650 ft/s) were covered by this study. The results showed that the compressors with the lower diameter had a higher efficiency, and although the compressor efficiency tended to increase with increasing axial velocity, the losses in the discharge diffuser increased rapidly enough to nullify this trend and encourage the selection of lower axial velocities.

The design chosen for the alternate two-stage design has an axial velocity of 111 m/s (365 ft/s) and a hub diameter of 914.4 mm (36 in.). The compressor efficiency is 89.3%, and the overall efficiency, including the discharge diffuser, is 81.7%. The remaining design point geometric and performance parameters are listed in Table 4-2.

4.1.2.2.3. Two-Stage Compressor Design for 4500 rpm. The second two-stage compressor design study was carried out to explore the possibility of using 4500 rpm drive motors. Maintaining the same pressure rise parameter at 4500 rpm as at 3600 rpm gives a hub diameter of 731.5 mm (28.8 in.), with the tip diameter depending upon axial velocity. Designs with axial velocities of 107, 122, and 137 m/s (350, 400, and 450 ft/s) were analyzed. The resulting outer diameters and efficiencies are given in Table 4-3. It can be seen from this table that although the compressor efficiency increases with increasing axial velocity, the larger velocity head at the diffuser inlet, coupled with diffuser efficiency, produces an increase in absolute energy loss and lowers overall efficiency. The tendency would therefore be to choose the design with lowest axial velocity still compatible with the maximum relative diffusion factor at the blade root. Determination of blade natural frequencies and a study of excitation frequencies must be carried out before a final selection can be made.

TABLE 4-3
MAIN HELIUM CIRCULATOR CONCEPTUAL DESIGN, CONCEPT "A" [TWO-STAGE
COMPRESSOR, 4500 RPM, HUB DIAMETER = 731.5 MM (28.8 IN.)]

Helium axial velocity, m/s (ft/s)	107 (350)	122 (400)	137 (450)
Outer diameter, mm (in.)	1116 (43.94)	1076 (42.366)	1044 (41.10)
Compressor efficiency, %	88.71	89.42	89.89
Overall efficiency, %	82.64	81.64	80.06
First stage centrifugal stress, MPa (psi)	83.67 (12,138)	73.28 (10,630)	65.2 (9,458)
Second stage centrifugal stress, MPa (psi)	83.67 (12,138)	73.28 (10,630)	65.2 (8,458)

4.1.3. Design Verification and Support

4.1.3.1. Introduction

The original purpose of the circulator tests was to substantiate the operational capability of the steam-driven main circulator machine assembly and its service system as required by and described in:

900829	Specification for 2000/3000-MW(t) Prototype Main Helium Circulator Test
TS-074	Phase I Tests for Main Circulator and Test Facility
902964	Phase I Main Circulator Test Procedure

Although the thrust of the work being performed under the Program Area reported here is concerned with an electrically driven circulator, testing of the steam-driven circulator will be resumed and continued to verify component designs and to determine the interactions of the circulator and the bearing auxiliary service systems, which will be very similar to systems required for an electric-motor-driven circulator. Experience will thus be gained in operation of the test facility at elevated temperatures and helium pressures, as well as in operation of the main helium shutoff valve and other systems.

The testing will consist of:

1. Verifying performance characteristics of the bearing system.
2. Establishing performance characteristics of the seal system.
3. Recording performance of circulator service system.
4. Monitoring and recording aerodynamic performance of the helium compressor, inlet, and diffuser.
5. Performing full-speed test.

Figure 4-5 shows an overall cross-sectional view of the circulator assembly installed in the test vessel. Figure 4-6 is a flow diagram of the circulator service system, showing the main system components as well as gas and water flows. Figure 4-7 shows a cross-sectional view of the circulator bearing and seal assembly.

4.1.3.2. Activities Related to Test Circulator

The circulator had previously been assembled and installed in the test vessel. However, the machine was incomplete since delivery of the shutdown valve assembly had been delayed and a non-functional dummy shutdown valve assembly had been installed. It was also known that the axial clearance in the thrust bearing was only 0.097 mm (0.0038 in.), whereas the drawing callout is for 0.23 ± 0.025 mm (0.009 ± 0.001 in.).

One short test run was performed at 2000 rpm with the steam loop filled with nitrogen. The test was terminated before reaching operating temperature in order to prevent potential damage to the circulator bearings. Difficulties were experienced in starting the circulator and achieving self-turbining under bearing water flow alone and metal cuttings and debris were found in the bearing water drain line filters. It was therefore decided to disassemble the machine, check internal passages for foreign matter, establish proper thrust bearing clearance by reworking components, and complete and install the shutdown valve assembly. A circulator disassembly procedure was prepared and the circulator was dismantled.

The thrust bearing clearance problem was traced to out-of-tolerance components. All discrepant components have been reworked to establish the proper thrust bearing clearances. The reassembly of the test circulator is under way and on schedule. All stainless steel fasteners suspect of galling have been oxidized and will be installed with Molykote paste lubricant in an attempt to avoid future problems. The dummy shutdown valve assembly will be replaced and the original shutdown valve assembly will be installed. Heat treating and final machining operations are being performed at the present time.

Fig. 4-5. Helium circulator assembly installed in test vessel

Fig. 4-7. Helium circulator bearing assembly

4.1.3.3. Summary and Planned Testing

Preliminary runs have been made to test buffer helium system, bearing water system, self-turbining, loss of bearing water conditions, and the shutdown procedures.

The following tests are currently planned:

Phase 1. High-speed steady-state runs combined with the circulator bearing system tests, initial aerodynamic tests, full-temperature operation, depressurized operation, and shutdown tests.

Phase 2. If the supplemental funding is approved, the following tests will be performed: emergency shutdown tests for loss of bearing water pump, loss of buffer helium, circulator trips, and rapid depressurization.

REFERENCE

- 4-1. "Configuration Evaluation Report," DOE Report GA-A14900, General Atomic Company, to be published.