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GAS-COOLED REACTOR PROGRAMS

HIGH-TEMPERATURE GAS-COOLED REACTOR SAFETY STUDIES PROGRESS REPORT

January 1, 1974 - June 30, 1975

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OAK RIDGE NATIONAL LABORATORY

OPERATED BY UNION CARBIDE CORPORATION FOR THE ENERGY RESEARCH AND DEVELOPMENT ADMINISTRATION

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PROGRESS REPORT FOR JANUARY 1, 1974, THROUGH JUNE 30, 1975

T. E. Cole and J. P. Sanders, Program Managers
P. R. Kasten, Program Director

Date Published: July 1977

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FOREWORD

The Gas-Cooled Reactor Programs being carried out at the Oak Ridge National Laboratory contribute to the development and evaluation of high-temperature gas-cooled reactors (HTGRs) and of gas-cooled fast reactors (GCFRs). The development programs that are sponsored by the U.S. Energy Research and Development Administration (ERDA) include the Thorium Utilization Program (HTGR Fuel Recycle Development Program), the HTGR Base Technology Program, the HTGR Safety Programs, and the GCFR Program. In addition to the ERDA programs, safety studies and assessments have also been performed at the request and under sponsorship of the Division of Systems and Safety and the Division of Reactor Safety Research of the U.S. Nuclear Regulatory Commission. These studies consisted of independent, objective assessments of the safety aspects of specific systems of power stations for which construction or operating licenses were being considered. This report documents the work performed under all phases of the HTGR safety studies at ORNL.

Major incentives for developing HTGRs are the prospects for economically attractive power production, improved fuel utilization, the potential for obtaining low environmental impact at a diversity of sites, the potential for high-temperature direct-cycle and process heat applications, and the pertinence of the component technology to GCFRs. Recognition of these incentives has led to the development, construction, and operation of several experimental and prototype nuclear power plants based on gas-cooled reactors that utilize various configurations of fuel, moderator, and gaseous coolant. More recently a number of construction-permit applications were considered within the U.S. for large central-station power plants based on a particular HTGR concept being offered commercially by General Atomic Company (GA). Much of the work for the Safety Program has concerned the study and assessment of the safety of these plants, such as the Summit Station for Delmarva Power and Light Company, and of their systems and components. Although plans for these power stations have since been cancelled for economic reasons, the original commitment on the part of the utility companies and GA represented a conviction that

HTGRs can be operated with a high degree of reliability and without undue risk to the health and safety of the public. The HTGR safety studies have as their objective to document the assessment and/or development of technology that will ensure appropriate levels of safety under postulated accident conditions that could affect either on-site or off-site personnel.

All tasks and assessments for the safety studies are coordinated closely with similar work at GA and at the Los Alamos Scientific Laboratory and Brookhaven National Laboratory, as well as with gas-cooled reactor development efforts in Europe. Significant efforts on HTGR safety-related work are being carried out at KFA in West Germany, and much work was conducted and reported by the Dragon Project in England before closure of the Project in March 1976. Before its closure, HTGR safety information was exchanged under the ERDA/Dragon HTGR Agreement.

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SUMMARY

Part I. HTGR SAFETY STUDIES FOR GAS REACTOR SAFETY BRNACH OF RRD, ERDA

1. SYSTEMS AND SAFETY ANALYSIS

1.1 Planning Guide for HTGR Safety and Safety-Related Research and Development

The subject planning guide was published in May 1974, after undergoing revisions by personnel of the Oak Ridge National Laboratory, General Atomic Company (GA), and USAEC (now ERDA). The guide is divided into three major parts: Part I provides general background information and discusses the need, objectives, and scope of the overall safety program. Part II explicitly delineates the research and development safety work by describing seven interrelated task areas. Part III presents a comprehensive description of the HTGR safety-related work.

1.2 HTGR Core Support Structure

The HTGR core support structure was analyzed for design adequacy and failure probability. Necessary research and development were identified to assure adequate safety margins.

The publicly available published material of the core support design was sufficient only for a preliminary analysis. Specific design criteria, details, and analyses were requested from GA. The information received is undergoing ORNL review.

Mechanics Research Inc. (MRI) of Los Angeles, California, was awarded a small contract by ORNL to develop a practical method of evaluating the failure probability of the core support structure for a single refueling region. No GA proprietary information was involved in this segment of the study. General reliability methods were described and applied to this first-of-a-kind design. The necessary input parameters were identified. Sample calculations revealed both a need for accurate input information and a significant sensitivity to input parameter variations. Further sensitivity studies were recommended.

MRI also submitted a brief study concerning oxidation effects on the core support structural strength. Mean strength reduction uncertainties were identified and listed.

1.3 Consequences of Core Support Structure Failure

The consequences of core support structure failure were identified in two limiting cases. The first failure case postulated that one refueling region slumped to the floor of the lower reactor plenum. The second case postulated that all refueling regions slumped to the floor of the lower reactor plenum.

Analysis of the first case assumed that the refueling region slumped undetected and the reactor continued operation at 100% power. Any resultant change in core reactivity would probably be small and was assumed to be undetected. The region loses forced coolant circulation, and the fuel elements heat up in less than 2 min to the temperature (1873 K) at which the fuel particle coatings begin to fail. The resultant fission product release would increase the circulating activity in the coolant to 3 times the design value and 15 times the normally expected value within 4 min after the accident initiates. This increase should readily result in a basis for reactor shutdown.

After approximately 10 min of full-power operation, essentially all the coated fuel particles in the slumped region have overheated and failed. The released fission products that migrate to the exclusion area boundary as a result of expected leakage result in a whole-body dose of about 10 mrem and a thyroid dose of 11 mrem. These results are far below the accident release limits of 10 CFR Part 100.

Analysis of the second case revealed that if all the core supports failed simultaneously, a reactivity insertion of approximately \$3.50 results. This is more severe than the considered incredible rapid rod pair ejection accident, and is judged to produce unacceptable consequences to the public. An exact, detailed analysis of these consequences was deferred until the magnitude of the failure probability can be estimated from the susceptibility studies.

1.4 Steam Generator Review

The materials and design philosophy used in the HTGR steam generator are being reviewed. The materials proposed for the HTGR steam generator are (1) austenitic type 304 stainless steel, (2) 2 1/4 Cr-1 Mo steel, and (3) Incoloy alloy 800H. The necessary material properties are tabulated.

Detailed inelastic analysis, necessary for high-temperature applications, requires the development of constitutive equations mathematically describing the phenomenological model of the material mechanical behavior. Sufficient tabulated data are available for inelastic analysis of type 304 stainless steel and 2 1/4 Cr-1 Mo steel. Constitutive

equations for these two steels have been developed and are currently being validated at ORNL. However, Incoloy 800H data are insufficient for an adequate inelastic analysis, and constitutive equations developed by GA need to be carefully validated.

2. FISSION PRODUCT TECHNOLOGY

2.1 Accident Hazard Potential of Key Nuclides

Iodine, cesium, and strontium require careful consideration if primary containment is breached and factors causing loss of retention of the source are present. Otherwise the secondary containment, performing as designed, can be expected to keep releases low enough that doses will not exceed the regulatory limit.

2.2 Iodine Adsorption and Desorption

The maximum desorption of iodine is estimated to be about 228 Ci during a design-basis depressurization accident (DBDA); this leads to a maximum estimated release to the secondary containment of about 308 Ci if the "design" gas content is added. The dose resulting from this release to the secondary containment is estimated to be only 0.31 rem assuming a $\chi/Q = 0.001$ for the atmospheric dispersion and a value of 0.002 for the fraction of the amount entering the secondary containment that is released to the atmosphere. If the assumed secondary containment release fraction is correct, isothermal desorption of iodine from the primary circuit surfaces in a DBDA would not result in an excessive dose.

2.3 Assessment of Cesium Transport Parameters

The hazard potential of individual fission product nuclides depends on the source inventories in relevant locations and on the possibility of transport during accident situations. The source inventories are derived from material transport and capacity properties that exist during normal operation. Consistent values of the needed parameters are not always available in the literature. The following points result from the assessment of cesium transport parameters:

1. isotherm control of fission product plateout under design conditions must be taken into account in the estimate of inventories for plateout and gas;
2. the release from saturated surfaces during the course of any postulated accident must be taken into account;
3. evidently an increase in surface temperature in the course of a postulated accident may become highly significant, as reported isotherms vary appreciably with temperature;

4. design basis assumptions as to oxidation of various surfaces must be established;
5. isotherms must be established for such oxidized surfaces;
6. increased partial pressures under conditions of isotherm control will also increase adsorption of any dust and thus augment the associated hazard potential.

3. PRIMARY COOLANT TECHNOLOGY

The effect of chronic low levels of steam ingress into the primary system on the corrosion and consequent strength loss of the core support posts were assessed. The assessment necessarily proceeds through the following three steps: (1) The impurity composition in the primary system is estimated as a function of a range of steady ingress rates of from 0.001 to 1.0 g/sec, both by means of an analysis of the Dragon steam ingress experiment and by a computer code, TIMOX, which treats the primary system as a well-mixed pot. (2) The core post burnoffs that result from 40-year exposures to these determined impurity atmospheres are then estimated with a corrosion rate expression derived from published ATJ corrosion rate data. Burnoffs were determined for both the core posts at the nominal and the maximum sustained temperature, estimated to be 90°C above nominal. (3) The final step involves assessment of the degree of strength loss resulting from the estimated burnoffs. An empirical equation was developed for this purpose and compares reasonably well with strength loss data for a number of different graphites and specimen geometries.

Steps (1) and (2) yield an error band of predicted burnoffs with varying ingress rate for both the nominal and hot core posts. Step (3) was used to estimate a burnoff range that may cause 50% strength loss — the maximum allowable — for both the nominal and hot core posts. A method was developed for estimating the probability for 50% strength loss for the general case of overlapping error bands.

The nominal core posts have 8% probability for 50% strength loss even at the maximum assumed 40-year ingress rate of 1 g/sec (corresponding to 850 vpm* total oxygen). The nominal core posts have 0% probability of 50% strength loss for ingress rates below 0.025 g/sec (20 vpm total oxygen). The hot core posts show significantly greater probability for 50% strength loss because of the increase in corrosion rate with temperature.

Thus, a major problem this study calls to attention is the potentially excessive loss of graphite strength in the hotter core post regions. It is recommended that a more accurate definition of the size, location,

* vpm = parts per million on a volumetric basis.

and temperature excess of the sustained core post hot zones, created by regional power peaking, hot-streaking, and nonideal coolant flow distributions, be obtained.

4. SEISMIC AND VIBRATION TECHNOLOGY

A review of the GA HTGR core design and design verification for seismic loading conditions has been completed. It was generally concluded that the program was thoughtfully planned and is being vigorously pursued. However, because of the extreme complexity of the core and core support structure and reactor shutdown systems, only approximate "practical" solutions of the analytical problems can be expected and will require experimental verification. The need was seen for completing the documentation of information required to develop specific recommendations concerning design criteria, experimental model studies, and analysis methods development.

5. CONFINEMENT COMPONENTS

An assessment is in progress of the state of the art of prestressed concrete reactor vessel (PCRVR) development. The study is divided into the following areas: (1) concrete properties and construction practices, (2) analysis methods, (3) model testing, (4) new materials development, (5) instrumentation, (6) prestressing, (7) liners, (8) penetration, (9) thermal barriers and cooling systems, (10) foundation and support systems. The text of this report is composed of summaries of the reviews that have been completed thus far.

6. PRIMARY SYSTEM MATERIALS TECHNOLOGY

6.1 Assessment of Methods Materials Technology for HTGR Primary System and Core Support Structures

Two reports were completed during this time period, ORNL/TM-4760, *Initial Assessment of the Status of HTGR Metallic Structural Materials Technology*, and ORNL/TM-5136, *Status Report on Structural Materials of HTGR Primary System Components*. The first provides an interim assessment of the status of technology of the metallic component of the HTGR primary system. Major components such as the steam generator and helium circulators — and their materials of construction — are described, and areas of uncertainty, particularly as they relate to the safety of the system, are identified. Ongoing research aimed at

the improvement and refinement of HTGR components and materials technology is summarized, and suggestions and priorities are given for present and future work. The second document reports on the continuing assessments of the status of technology of primary system structural materials and implications of these in the prediction of component integrity and performance as related to safety. Relevant information on the metallic materials used in the primary system is provided along with initial assessment efforts on the core lateral restraint structure. No new materials-related safety questions of great urgency were encountered in the course of these studies, but several items needing further consideration and investigation were identified.

6.2 Assessment of Materials in HTGR Reactivity Control and Shielding Components

The second report cited the results of the initial assessment efforts on the control rods, burnable poison rods, reserve shutdown system, boronated shield, and upper plenum elements. The B_4C and graphite constituting these components will oxidize in the presence of some of the impurities contained in the primary coolant helium. Because of the uncertainty of possible transport and reactor effects an in depth analytical study is recommended together with continued examination and evaluations of material removed from the Peach Bottom Reactor and material that will become available in the future from Fort St. Vrain.

6.3 Assessment of Core Support Post Graphite

Four primary concerns were postulated relative to the in-service behavior and degradation of properties of the core support post graphite: (1) compatibility with ceramic and metallic components of the lower seat assembly, (2) irradiation damage, (3) fatigue and crack propagation behavior, and (4) oxidation by impurities in the primary coolant.

The first concern relates to possible reactions of graphite posts with Si_3N_4 (or other) seat material. This could lead to premature local failure. The design is not yet final; however, this question should be recognized and used as a criterion in design. The fast neutron dose to the core support posts ($\sim 10^{20} \text{ n/cm}^2$) is not expected to give trouble from dimensional or property changes, but irradiation might increase the graphite oxidation rate. We sought quantitative estimates of this potential effect. The third concern is the possible failure due to initiation and growth of cracks under both static and cyclic loading. Although not possible at present because of a lack of data, information on fatigue and crack growth due to loading and

environmental effects on crack behavior should be considered in design and end-of-life assessments. Long-term graphite oxidation and strength loss have also been pursued. Future assessment should involve definition and/or consideration of a number of factors including the maximum sustained graphite temperature, corrosion environment and rate, and strength loss burnoff correlations.

7. SAFETY INSTRUMENTATION

Preliminary results are presented of an ongoing study to assess the adequacy of and identify development requirements for safety instrumentation for large HTGRs. The object of the review is to identify areas where further development and design efforts would result in improved safety, reliability, or availability. The base of information being used for the review comes from Safety Analysis Reports for proposed HTGRs, topical reports, and published articles when available. In addition, comments have been informally obtained from GA on several points. The study to date has concentrated primarily on moisture monitoring instrumentation, but has also included in-core instrumentation and system logic configurations for the reactor shutdown system and engineered safeguard systems. Several recommendations are made for possible development efforts.

PART II. HTGR SAFETY STUDIES FOR THE GAS-COOLED REACTORS BRANCH OF THE DIVISION OF REACTOR LICENSING, USNRC

8. ANALYSIS OF LOSS OF FORCED CONVECTIVE COOLING ACCIDENT USING THE HEATUP CODE

Development work on the HEATUP computer program for the thermal analysis of Loss of Forced Circulation (LOFC) accidents in HTGRs was essentially completed, and a user's manual was drafted. The code was used in the thermal analysis of LOFC accidents involving both the Summit Power Station and the Fulton Generating Station.

9. DEVELOPMENT AND USE OF THE COUPLED CONDUCTION-CONVECTION MODEL FOR CORE THERMAL ANALYSIS

The capabilities of the Coupled Conduction-Convection Model (CCCM) computer program were extended to allow a thermal analysis of three-dimensional cylindrical models. The CCCM computer code was used in a thermal analysis involving a single blocked coolant channel in a fuel element. A parametric study analyzed the effects on fuel and coolant temperatures due to variations in the power level, coolant mass velocity, and coolant inlet temperature. The effects on the maximum

fuel temperature due to a totally closed orifice in the highest powered refueling region were analyzed. An analysis was begun for the thermal response of the Fort St. Vrain HTGR following a design-basis depressurization accident with operation of two circulators at 7000 rpm.

10. DEVELOPMENT OF THE MULTICHANNEL CONDUCTION-CONVECTION PROGRAM, HEXEREI

The HEXEREI code is a hexagonal and cylindrical mesh conduction and convection heat transfer code to be used in the thermal-hydraulic analysis of HTGRs during full power and during depressurization accidents. The code at present is capable of core transient and steady-state conduction cases. Development of coolant temperature and flow redistribution subroutines are in the debug stage and will be included into the main code in the near future.

11. COOLING SYSTEM PERFORMANCE AFTER SHUTDOWN

Computational methods were developed to determine the heat removal capability of the core auxiliary heat exchangers (CAHE) for large HTGR designs. The capability was evaluated for both the pressurized condition and for the conditions following a depressurization accident. The latter calculations required determination of the transport properties of mixtures of helium and nitrogen containing carbon monoxide. Such mixtures would result from the ingress of air from the containment building. These methods were used to determine the performance of the CAHE designs for both the Summit Power Station and the Fulton Generating Station.

12. EVALUATION OF CIRCULATOR AND MOTOR FOR THE CACS

We studied the auxiliary circulator and motor capabilities for the core auxiliary cooling system (CACS) for large HTGRs. The relationship describing the pressure loss around the loop was determined and used, in conjunction with specified operating characteristics of the circulator, to determine the total flows during the design-basis depressurization accident (DBDA) plus loss of main loop cooling (LOMLC). For the conditions specified by the vendor, GA, the performance specifications appeared to be adequate; however, independent computation of the air ingress rate following depressurization indicated a deficiency in the torque provided by the motor drive.

13. DEVELOPMENT OF THE CORE AUXILIARY COOLING SYSTEM CALCULATION, ACHERONS

A system of subroutines to provide independent analysis capability of the Core Auxiliary Cooling System was developed. These are to be included into the HEXEREI computer code in the near term to provide the heat duty, pressure drop, and exit temperature of the heat exchanger and the mass flow rate and pressure rise in the circulator. Results of depressurization accidents on the Fort St. Vrain and Fulton reactors are compared with results presented by GA.

14. DEVELOPMENT OF THE FLODIS CODE

In the determination of the maximum temperatures developed in the core and in the exit gas streams for the Fort St. Vrain (FSV) reactor during a specified depressurization accident, an accurate estimate of the distribution of coolant flow among the various refueling regions was needed. During full-power operation, the flow is adjusted by means of a movable orifice so that the exit temperature from each region is the same. At low flows, the orifices are no longer effective, and the total flow is redistributed among the regions in accordance with relative flow paths and heating rates. A computer program, called FLODIS, was written to determine this distribution of flow during the transient.

15. AIR INGRESS RATES TO THE PRIMARY SYSTEM FOLLOWING A DBDA

A computational method was established to estimate the exchange rate between air in the containment building of a large HTGR and the helium in the primary system following a design-basis depressurization accident (DBDA). The computation involved a number of assumptions concerning the chemical reaction between entering air, mixing effects within the primary system, and temperature distributions in both the primary system and the containment structure. In addition, detail input information concerning the relative volumes of the two systems, final system pressures, and break size were required. This process is important only when the break occurs in the top of the primary system boundary, and it appeared that the most critical input variable was the effective buoyant head causing flow. When input values similar to those indicated by GA were used, the computed results agreed well. A disagreement remained, however, on the value of the effective head.

16. PERFORMANCE OF THE THERMAL BARRIER COVER PLATES AT THE LIMITING TEMPERATURES

Square and hexagonal plates which are typical of cover plates for the PCRV of the HTGR were analyzed for severe transient conditions to determine the resulting accumulated elastic, plastic, and creep strains by use of the inelastic finite element computer program EPACA. The total strain in the Hastelloy X square plate was within the "Damage Limit" and "Critical Safety Limit" set by GA. However, deflection of the plate is greater than 25 mm (1 in.), allowing separation of the insulation from the plate, thereby failing to achieve one of the design objectives. The General Atomic Company specified hexagonal plates of carbon steel. Since no carbon steel data were available at the specified elevated temperatures, 2 1/4 Cr-1 Mo steel was substituted to give some indication of the behavior of a low-alloy steel for which property data are available. Although the accumulated strain and deflection for the 2 1/4 Cr-1 Mo plate were found satisfactory, the integrity of carbon steel plates for the specified conditions would be very doubtful.

17. EVALUATION OF TEMPERATURE LIMITS FOR THE FAILURE OF FUEL PARTICLE COATINGS

In the evaluation of critical safety limits given by GA, both damage and critical safety limit temperatures for the failure of the fuel particles are given. Certain limiting temperatures are obvious, such as the graphite sublimation temperature [$>3300^{\circ}\text{C}$ (6000°F)], and the temperature [about 2400°C (4300°F)] at which the fuel particle melts. Other failure points are not as well defined. The graphite matrix fails at about 1650°C (3000°F); however, this does not represent a critical safety point since cooling can still be maintained. It does result in sufficient damage to prevent further operation without fuel replacement. Failure of the particle coating results from a combination of effects such as differential thermal expansion, irradiation damage to the structure, buildup of internal fission-gas pressure, and possible chemical interactions within the coating. All these are significant for isothermal operation of the particle; when a thermal gradient exists, kernel migration will add to the failure mechanisms. Within the constraints of these processes, combined effects can be considered, and a probable failure zone can be indicated.

18. ANALYSIS OF TRITIUM DISTRIBUTION AND RELEASE IN HTGR

Tritium will be produced in HTGRs not only by fission but by reactions of neutrons with boron in the control rods, with trace lithium impurity, and with ^3He in the helium coolant. A computer program was written to determine the tritium inventory in various sections of the

plant as a function of the operating history of the plant, helium purification rate, and constants related to the plant design. This program is called TRITGO; it has been documented, and test cases have been executed.

19. ENERGY RELEASE TO PCRV DURING DBDA

The total energy released to the containment of the Summit Power Station during a depressurization accident was estimated. The computation was made to evaluate the response given to question 6.30 in the PSAR for that plant. The approach was to estimate on the basis of energy transport rather than energy transfer. It was concluded that the total energy that could be transported to the containment system was 12.9 GJ (12.2×10^6 Btu). This agreed well with the quantity indicated by the utility; in fact, two items contributing to the conservatism of the response were cited.

Part III. HTGR SAFETY STUDIES FOR THE DIVISION OF REACTOR SAFETY RESEARCH, USNRC

20. CORE SIMULATION FOR EMERGENCY COOLING ANALYSIS

A computer code ORECA, an ORNL approximation of the GA RECA code for simulating Reactor Emergency Cooling Accidents, was developed to analyze DBDA and LQCA transients. Separate versions of ORECA were made for each of the three reactor types [Fort St. Vrain (FSV), 2000 MW(t), and 3000 MW(t)]. Of the three, the FSV version is the most highly developed, and example transients are presented.

21. CORE MODEL FOR USE IN OVERALL NUCLEAR STEAM SUPPLY TRANSIENT ANALYSIS

A coupled heat transfer and neutron kinetics core simulation code for the HTGR has been developed and incorporated into an overall nuclear steam supply system response program. The core simulation code calculates the temperature distribution in an average fuel rod and the surrounding graphite moderator and coolant channels and has the capability to determine temperatures in the "hot" fuel column, the surrounding graphite moderator, and coolant channels. The neutron kinetics behavior of the core is modeled by use of the space-independent kinetics equations with six groups of delayed neutrons. The heat transfer calculations and the neutron kinetics calculations are coupled by an iterative procedure.

22. HTGR REHEATER AND STEAM GENERATOR MODEL DEVELOPMENT

A reheater-steam generator dynamic simulation code has been developed and incorporated into an overall nuclear steam supply system response code. The code simulates the reheater-steam generator module with a multinode, fixed-boundary, homogeneous flow model. The code solves the conservation of energy, mass, and momentum equations for the tubes by an implicit integration technique. Equations of state, heat transfer correlations, and friction factor routines are included in the code. Steady-state results obtained with the code are in reasonable agreement with data given by GA.

23. TURBINE-GENERATOR PLANT SIMULATION

A detailed dynamic simulation of the turbine-generator plant for the Delmarva Summit reactor was developed, debugged, and prepared for incorporation into an overall nuclear steam supply systems code.

24. CORE AUXILIARY COOLING SYSTEM MODEL DEVELOPMENT

Models for both the Core Auxiliary Heat Exchanger (CAHE) and Core Auxiliary Cooling Tower (CACT) have been developed, and simulations of both are operational.

25. STEAM-GENERATOR DYNAMICS MODELING

Work on steam-generator dynamics modeling has focused on several areas: evaluation of both linearized and nonlinear moving boundary models; development of a detailed model for the Fort St. Vrain (FSV) reactor steam generator to use in planning and interpreting dynamics tests; and development of a nonlinear, fine-mesh, fixed-node model.

26. DIGITAL COMPUTER CODE ACQUISITION AND EVALUATION

The first program released by GA was the POKE code; it was made operational at ORNL. The conversion of this code was relatively straightforward; it involved only the substitution of equivalent FORTRAN statements. An earlier version of the kinetics code, BLOOST-5, was available from the Argonne Code Center in an IBM-compatible package. Transients executed with the BLOOST code compared well with the single-channel, TAP-type core model developed at ORNL. The RECA program

depends upon exercising the CINDA-3G preprocessor to execute different problems. The CINDA-3G preprocessor produces a "packed," machine-language tape that is required as input to the execution of RECA. Since the structure of the tape depends upon the "hardware" of the computer, a program was written at ORNL to rearrange this tape into the form that is compatible with the IBM-360 system.

Part I. HTGR SAFETY STUDIES FOR GAS REACTOR
SAFETY BRANCH OF THE DIVISION OF REACTOR RESEARCH AND DEVELOPMENT

T. E. Cole, Program Manager

This section of the report covers work performed on the HTGR Safety Program under sponsorship of the Division of Reactor Research and Development of the USAEC (now ERDA). The organization of the chapters is based on the division of work as outlined in the *Planning Guide for HTGR Safety and Safety Related Research and Development*, ORNL-4968, which was prepared at the request of the USAEC and published in May 1974. This Planning Guide divides the work into seven task areas; the first of these addresses all aspects of systems and safety analysis for HTGRs, and the work devoted to this task is reported in Chapter 1. The remaining six technology task areas identify activities that are reported in Chapters 2 to 7; chapter headings in each case are identical with the related task areas identified in the Planning Guide. In each chapter the work reported represents a detailed assessment of the safety of particular items of HTGR technology, and the identification of areas of safety-related technology for which additional development work is indicated.

1. SYSTEMS AND SAFETY ANALYSES

Dunlap Scott

Following the completion of the Planning Guide, Task Area I of the HTGR Safety Program was directed toward the examination of the performance of components critical to the safe shutdown of the plant during and following the initiation of the accident sequences described in the HTGR safety analysis reports. The accident analyses in these reports make certain assumptions regarding the continued operation of these components throughout the accident sequence; functional redundancy is required in those areas where single failures would cause significant consequence to the accident progression. The assumptions of continued operation or the effectiveness of redundancy would be made invalid by common mode failures, which might be caused by the additional loads imposed on the system by the forces produced in the accident sequence. Our initial effort was to identify the critical safety components and the accident sequences that were likely to produce such failure. These are listed below:

1. steam generator system — DBDA (design basis depressurization accident) and steam ingress;
2. core support structure — seismic event, water ingress;
3. core auxiliary cooling system — DBDA;
4. reactivity control system — DBDA and LOFC (loss of forced cooling);
5. PCRV liner and penetration system — DBDA, LOFC, steam ingress.

Two of these, the gross failure of the steam generator under DBDA conditions and the risk analysis of failure of the core support structure under seismic conditions, were identified by AEC as being studies of first priority, with particular emphasis placed on a best effort estimation of risks. A significant criterion relating risk to potential key public safety concern was established by RDD to govern the decisions as to where gas reactor safety research and development would be directed. Accidents that could constitute a public risk on the order of or greater than the current upper limits of the envelope risk values as given in the AEC's Reactor Safety Study¹ or other such overall reactor

safety risk assessments were defined to be of key public safety concern. These accidents would receive further study to resolve any uncertainties or to examine design options that reduce either the likelihood or the potential consequences. The accident sequences of significant risk identified in the Reactor Safety Study¹ included failure of the primary containment resulting in gross release of fission products. Therefore, we included in our scope of study the challenge to the containment caused by such accident progressions.

The studies to determine the probability of failure of critical safety components usually involved first-of-a-kind components for which experience data are lacking or concerned the structural reliability, for which limited methodology exists. A short study by a consultant indicated a possible methodology for estimating the probability of structural failure while including such parameters as random seismic forces, the statistical distribution of strength of materials, and deterioration of the component properties during operation. While the study showed much promise for the method itself, its main impact was to relate the importance of good statistical data in properly characterizing the properties of the materials used in the components considered critical to safety. Additional studies are planned to follow up on the interesting features of these studies.

1.1 PLANNING GUIDE FOR HTGR SAFETY AND SAFETY-RELATED RESEARCH AND DEVELOPMENT — J. R. Engel

The preparation of a proposed HTGR Safety Program Plan, which included input from the General Atomic Company and the USAEC, was completed in 1973. The plan was originally intended to include research, development, and analysis in all phases of HTGR technology that relate to the evaluation of HTGR safety. Following review of the preliminary draft, a revised version was presented to the USAEC in December 1973. Review of this draft by the USAEC resulted in the determination that most of the research and development activities identified in the plan properly belonged in the HTGR base program. Revision of the draft into two parts to separate safety and safety-related tasks was undertaken early in 1974 in order to make generally available the detailed

review of technology contained in the plan, while significantly redefining the scope of the program itself. The resulting report² was published in May 1974.

The document is divided into three major parts. Part I provides general background information for the reader who is not familiar with HTGRs and their safety aspects. It begins with a detailed discussion of the need, objectives, and scope of an overall safety program for HTGRs. This is followed by a general description of a steam-cycle HTGR system and a survey of the safety considerations associated with this system. Also included is a summary of the postulated accidents that have been examined and their expected consequences.

Part II provides a planning guide for research and development work directed explicitly to the safety of HTGRs. This part describes the work currently identified as safety work and the priority assignments, costs, and schedules estimated for that work. Seven interrelated task areas are identified as follows:

1. systems and safety analysis,
2. fission-product technology,
3. primary coolant technology,
4. seismic and vibration technology,
5. confinement components (concrete reactor vessel and containment structures),
6. materials technology,
7. safety instrumentation.

Within each task area, task groups are designated to define the studies to be performed. An important aspect of the safety work in each task area is a comprehensive assessment of the current state of the technology and a definition of additional needs; in some areas this is the only work that is currently identified. However, additional safety tasks may be added in any of the task areas as additional information becomes available.

Part III presents a comprehensive description of the safety-related work, as well as the safety work, that was identified with HTGR technology, with the exception that task area 1 is not repeated in

Part III because it is treated in its entirety in Part II. For the remaining six task areas, Part III gives both safety-related engineering and technology work and the previously identified safety work to clarify the interrelations among the various tasks. However, it is presumed that most, if not all, of the safety-related work will be carried on outside the HTGR safety program.

1.2 HTGR CORE SUPPORT STRUCTURE — I. T. Dudley

The objectives of this task are to analyze the HTGR core support structure design for susceptibility to failure, estimate the probability of its failure, and evaluate the safety and related technology to identify needs for additional research and development work to assure adequate safety margins for the HTGR concept. This work is an integral part of the overall ERDA program supporting the development of the HTGR concept.

The overall risk associated with the core support structure is defined as the product of the consequences of failure and probability of failure. Because this is a first-of-a-kind design, experience numbers are not available for estimating the probability of failure, and it was necessary to develop methods and techniques for analytically determining stress-strength probability distributions and the statistical methods for combining those distributions to provide an overall assessment of the core support structures reliability throughout its design life.

The HTGR uses helium for the reactor coolant and graphite as the moderator and core structural material. Fuel elements are graphite hexagonal blocks stacked in columns in a massive cylindrical array. The active core region is surrounded by graphite reflector blocks, and the entire assembly is supported by graphite structural members (see Fig. 1).

Helium coolant, circulated by axial flow compressors, flows downward through the core and reaches a normal exit temperature of approximately 760°C (1400°F). At that point it enters the lower plenum provided by the core support structure, flows radially outward around the graphite

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Fig. 1.1. General Reactor View. General Atomic Company drawing.

support posts into ducts in the prestressed concrete reactor vessel (PCRv), which then direct it upward through the steam generators. The axial flow compressors, located on top of each of the steam generators, then drive the coolant back into the upper plenum and then down through the core. Reactor coolant system pressure is maintained at approximately 4.8 MPa (700 psia) within the steel-lined PCRv.

The core support structure must provide a stable platform on which the fuel elements and reflector blocks are stacked. The support structure must support and maintain the core and reflector assembly within definite vertical and lateral limits for all conditions. It must also provide a passage for the reactor coolant leaving the core. The General Atomic Company (GA) design of the core support structure uses graphite core support blocks, each of which supports a core refueling region (an array of seven fuel element columns), graphite permanent reflector support blocks of various configurations to match the permanent reflector blocks, graphite upper post seat inserts, graphite posts, graphite lower seats, and ceramic lower pads. The lower seat and ceramic pad assembly is supported by the PCRv floor liner. Horizontal positioning and restraint of the core and reflector assembly is maintained by spring pack assemblies attached to the PCRv liner wall.

Each of the support blocks sits on either three or four posts as determined by position or load. Spherical post ends and seats are designed to permit a limited amount of lateral movement of the support blocks in any direction. This arrangement accommodates lateral movements due to thermal expansion of the blocks and provides a passage for the coolant leaving the core.

Pads attached to the spring packs fit into vertical slots in the outer surfaces of the permanent reflector blocks. This design permits vertical thermal expansion of the core and reflector assembly yet restrains radial and rotational movements.

Two grades of graphite have been identified by GA as reference design materials for use in the core support structure. They are UCC-PGX for support blocks and UCC-ATJ for the support posts and seats. Other graphites of similar physical characteristics might be used. Grade PGX graphite is a medium-grain material developed for optimum

structural qualities in large parts. Grade ATJ is a very fine-grain, high-strength graphite. The main advantage of graphite is its retention of strength at high temperatures.

The ability of the core support structure to function reliably over the 40-year design life of the reactor is obviously essential. This study of the GA design has been based on all available published information and unpublished design details, analysis, and other information supplied by GA at our request. All unpublished information supplied by GA has been identified by GA as being proprietary and has been treated accordingly here at ORNL by mutually agreed upon procedures.

It was not intended that an independent design or design analysis be generated by ORNL. That would have been neither practical nor within the scope of this task.

1.2.1 Procedure

The main sources of published material on the GA core support structure design were the preliminary safety analysis reports for the Delmarva and Summit plants, the *General Atomic Standard Safety Analysis Report*³ (GASSAR-6), and transcripts of meetings of the Advisory Committee on Reactor Safety. The information contained in those documents was sufficient to permit only a preliminary analysis, which revealed areas of special interest and need for more specific information about the GA design. The following is a list of the information of special interest, which was requested of GA:

1. core support structure design criteria;
2. the design details and engineering analysis of the core restraints that prevent rotation of the core about its vertical axis;
3. design details and engineering analysis of the radial restraint for the outer core support blocks;
4. procedures and results of ultimate load tests performed on (a) all core support block configurations, (b) upper post seats, (c) posts, (d) lower post seats, and (e) insulating ceramic bases;
5. the maximum normal operating load supported by a single core support block (some of the outer support blocks appear to be larger than

the inner blocks and would therefore be required to support greater loads);

6. design details and engineering analysis of (a) the upper post seats, (b) posts, (c) lower post seats, and (d) ceramic bases;
7. details of the restraints for the ceramic bases;
8. physical and chemical properties of the ceramic bases;
9. design details and engineering analysis of the supporting system for the segmented core barrel, spring packs, boronated graphite shield, and peripheral seal;
10. design details and engineering analysis of the spring packs;
11. analysis of the effects of high contact stresses on oxidization rates and strength deterioration of the upper and lower post ends;
12. support post installation procedures with regard to height adjustments and alignment (should include the bases for the steps in the procedure);
13. the specifications for the grouting beneath the PCRV liner plate with regard to size and number of voids that are acceptable, including the engineering basis for the specification;
14. plans for in-service inspection of the posts, post ends, post seats, and ceramic bases, including description of access to the various areas of interest and discussion of the adequacy of the inspection method;
15. mechanical properties of the materials to be used in the posts, post seats, and bases. These should include modulus of elasticity, creep strain rates at temperatures of interest, and uniformity of these properties across the materials.

1.2.2 Reliability Pilot Study

In addressing the problem of reliability, Mechanics Research, Inc. (MRI) of Los Angeles, California, with an office located in Oak Ridge, Tennessee, was given a small consulting contract by ORNL for the development of a practical method for evaluating probability of failure of a single refueling region core support structure. Their work was based mainly on information contained in GASSAR-6, and no GA proprietary information was involved.

The results of the pilot study to investigate the application of reliability analysis methods to the core support structure were presented⁴ in an MRI report to ORNL. This study was performed as an example of how reliability methods may be applied to first-of-a-kind structural components. General reliability methods, applicable to other components, are also described. The application of these methods to the core support structure is addressed qualitatively to identify significant factors to be considered, information needs for detailed analysis, and means for obtaining the required information. The actual numerical examples generated in the study were based on simplified analytical models and are for illustrative purposes only. Several random variables were considered, including core support strength at start of operation, strength reduction with time, random variation in loads applied to the structure during normal operations, and transient seismic loads. A computer program using Monte Carlo simulation techniques was used in performing the reliability calculations.

The sample problem considers the failure of the support for one refueling region. One model considers the failure of three support posts only, and in the second version the effect of failure of a support block is also considered. The numerical results presented are illustrative examples only of the potential of the method and may not be realistic because of the assumptions required to compensate for the limited structural strength data available. While these numerical values may not be realistic, the results are useful in examining the sensitivity of the probabilities of failure to variations in a number of the important parameters. For example, the probability of failure is dramatically increased when the strength loss is changed from 30 to 50% at the end of 40 years life. Accurate estimates of the mean rate of strength reduction are therefore essential in obtaining realistic reliability estimates. In another example, the reliability was reduced by about 2.5 orders of magnitude in reducing this factor of safety from approximately 8 to approximately 5.

The significant differences in reliability obtained for the various cases analyzed point out the need for further sensitivity analysis.

For example, the following parameters should be varied to establish their effect on reliability:

1. standard deviations (initial strength, rate of strength reduction, and post-trial load);
2. frequency of earthquakes;
3. lower limit on initial strength (to investigate the effect of proof testing).

Also, additional investigations should be performed to reduce uncertainties in the estimates for those parameters that most significantly affect reliability (e.g., mean rate of strength reductions).

Mechanics Research, Inc. also performed a brief study of the effects of oxidation on the core support structural strength. Emphasis is given to the significant uncertainties with regard to the mean strength reduction that will take place in the core support structure. Sources of uncertainty in the mean strength reduction are listed as follows.

1. Test data for graphites to be used in the core support structure of HTGRs were not available then.

2. Current burn-off predictions vary considerably, depending on the reaction rate expression assumed.

3. The current burn-off predictions do not account for effects of surface tensile stresses or irradiation on burn-off.

4. The available test data for strength is for higher temperatures than predicted for the core support structure.

5. The available test data are not directly applicable to the core support structure.

Work is continuing at ORNL on the analysis of the core support structure for its susceptibility to failure. This includes a review of GA analyses, design safety factors, effects of graphite strength deterioration, and other design details that affect the core support structure reliability.

1.3 CONSEQUENCES OF CORE SUPPORT STRUCTURE FAILURE IN HTGRs

1.3.1 Reactivity and Temperature Effects — J. C. Cleveland and S. J. Ball

Initial investigations were made concerning the consequences of postulated core support structure failures in an HTGR. The subject of determining the probability of failure of the core support posts is addressed in Sect. 1.2.

Each refueling region in an HTGR rests on a core support block, which is supported by three core support posts, as shown in Fig. 1.2. These posts are approximately 1.8 m (6 ft) in length and 0.15 m (6 in.) in diameter. Each support block supports seven columns of eight hexagonal fuel elements each plus the top and bottom reflector elements. This analysis considers the consequences of failure of the posts supporting one or more support blocks, resulting in one or more refueling regions slumping to the floor of the lower plenum.

The analysis was divided into two cases:

1. failure of three posts supporting one refueling region and the subsequent slumping of that region to the plenum floor,
2. failure of all posts and subsequent slumping of all refueling regions to the plenum floor.

In the first case (illustrated in Fig. 1.3), the question first considered was: "How would this failure be detected?" Since the exit from each refueling region contains thermocouples to monitor the region outlet gas temperature, these thermocouples may fail if the region begins to slump. However, this failure might not offer sufficient evidence to force an immediate shutdown. The accident might also be detectable if a significant change in core reactivity results from the downward movement of one refueling region. However, we believe that the change in reactivity could be quite small because of the counter-acting effects of:

1. the negative component of reactivity due to fuel moving away from the rest of the active core;
2. the negative component of reactivity due to increased neutron leakage; and

Fig. 1.2. Vertical Section — Reactor Core. General Atomic Company drawing.

Fig. 1.3. Refueling Region Support Failure.

3. the positive component of reactivity due to the higher fissile concentration in the upper half of the stack of fuel elements moving downward into the higher importance region of the core.

Furthermore, the slumping could occur over an extended time period — perhaps hours or days. The corresponding slow reactivity change could be compensated by slight movement of the regulating rods during this period and therefore go undetected. A two-dimensional transport theory calculation performed by Kirk⁵ analyzing the reactivity change caused by the slumping of the central refueling region indicated a reactivity loss of about 4¢. [This compares to a regulating rod pair worth ~16¢/m (5¢/ft).] That work thus supports the assumption that the reactivity change could go undetected.

To determine if the accident could be detected from coolant fission product activity, the fuel temperature transients in the slumped region were calculated by assuming that the reactor continued to operate at

100% power. A special variation of the ORECA code⁶ was used, with the assumptions peculiar to these calculations listed below:

Core conditions

1. continued 100% power operation following support post failure,
2. constant core inlet helium temperature and flow rate,
3. before slumping, helium flow rate through each region proportional to its radial power density,
4. highest powered region slumps,
5. instantaneous slumping with no flow through slumped region.

Core temperature lumping

1. ten axial nodes for each of 54 refueling regions,
2. more detail for slumped region — one node per fuel element (56 total) plus 14 top and bottom reflector nodes,
3. both radial and axial heat conduction allowed.

The results of the transient heat-up calculations are shown in Fig. 1.4. We see that the temperature at which fuel particle failure is assumed to begin (1873 K) is reached in less than 2 min in the slumped region. Few, if any, of the coated fuel particles will remain intact at 2273 K. Therefore, after about 10 min of continued 100%-power operation, essentially all the coated fuel particles in the slumped region will have failed and thus released fission products. Other calculations showed that a scram at any point in the transient would effectively halt the rise in temperature.

The results of this calculation were then used to determine the resultant coolant circuit activity and dose at the exclusion area boundary. As follows in Sect. 1.3.2, within 1 min after the failure temperature of 2273 K is exceeded, the coolant activity would at least triple its value if the reactor has been operating with the design limit of coolant activity, and it would increase by a factor of 15 if the reactor were operating with expected activity. This large and rapid increase in coolant activity would therefore provide an obvious signal to force an immediate reactor shutdown.

The calculated dose at the exclusion area boundary was found to be much less than the accident release limits of 10 CFR Part 100.

Fig. 1.4. Fuel Particle Transient Heat-Up.

The second case considered — failure of all or many core support posts — is of concern because a large reactivity insertion is possible. Because the control rods are supported from above by cables, the core could fall a distance approximately equal to the length of the core support posts, 1.94 m (6 ft 4.5 in.), with the control rods remaining stationary. Furthermore, possible core rearrangement could prohibit further insertion of the control rods (and the reserve shutdown spheres).

The worst case conditions assumed are as follows:

1. core falls rapidly,
2. core rearrangement prohibits further control rod insertion and insertion of reserve shutdown spheres,
3. core falls from hot full-power condition.

In terms of reactivity addition, this accident is the same as removing all rods that are fully or partially inserted by approximately 1.8 m (6 ft). The reactivity addition was estimated by considering the reactivity held by the rods and factoring in the fractional insertion of the rods. An integral rod worth curve was then used to estimate the loss in reactivity hold-down when the core falls.

The amounts of reactivity held by rods under various conditions are:

<u>Condition</u>	<u>Excess Reactivity, Δk</u>
Beginning of cycle, before buildup of fission products	<0.07
After ^{135}Xe , ^{233}Pa , and ^{149}Sm have reached equilibrium	≤ 0.025
Following prolonged middle of cycle shutdown	≈ 0.10

From an integral rod worth curve it can be shown that if core excess reactivity $\Delta k = 0.025$ (for instance) is held by a rod bank that is approximately 60% inserted into the core, approximately $\Delta k = 0.020$ will be added during the accident, since the rod bank will be only 30% inserted after the core falls 1.8 m (6 ft).

The reactivity addition of about \$3.50 (which could be considerably larger if a larger core excess reactivity were available), if added rapidly, becomes more severe than the rapid rod pair ejection accident, an accident that was analyzed³ in GASSAR-6 but was not considered credible. The results of a collapse of most of the refueling regions is judged to present unacceptable consequences to the public. A very complicated analysis would be required to estimate the sequence of events and analyze the consequences. This analysis was deferred until the likelihood of such a failure can be estimated from the susceptibility studies.

In summary, preliminary scoping calculations indicate that the failure of the support posts and slumping of one refueling region would not present a significant safety problem, and that the simultaneous slumping of several refueling regions would be needed before the guidelines of 10 CFR Part 100 would be challenged.

1.3.2 Fission Product Effects Associated with Hypothetical Failure of Core Supports — E. L. Compere

A scenario of a postulated hypothetical accident sequence suggested that one or more refueling regions might slump downward because of support failure, without specific indication to the operator. Thus power might

be maintained while coolant flow is lost to the elements involved. In the absence of cooling with continuation of power, the temperature would increase at a rate of about 400°C/min, and fuel temperatures would begin to reach 2000°C in about 3 min (Fig. 1.4). Few if any coated particles would remain intact at such a temperature.⁷

The transient release behavior of zoned cores in the presence of slower but similarly extensive temperature increases caused by after-heat has been considered by the SORS code.⁸ These codes could be adapted to the present problem, but differences in the situation considered made preliminary scoping calculations of interest.

The failure of the core support structure for a refueling region of fuel elements, with loss of forced cooling during operation, was characterized by the following special factors:

1. a limited number of fuel elements (7×8) is involved;
2. power operation continues, including purification system operation;
3. no breach of primary system containment is indicated, so only usual leakage is presumed;
4. because accident signals have not occurred, the usual atmosphere turnover of the containment ventilation system is implied;
5. because long-term meteorological averaging is not applicable, only worst case worst sector dispersion factors are used.

Because reactor operation is postulated to continue as the column of fuel elements becomes overheated, more fission products could enter the coolant circuit. Hence

1. release from the plant could increase;
2. increase in coolant activity might provide a timely indication of the situation.

These points are considered below.

1.3.2.1 Dose Effect of Increased Plant Release During Normal Operation

The maximum dose at the exclusion area boundary resulting from complete release of the full inventory of one fissioning fuel element into the primary coolant is estimated for important nuclides as follows.

$$\text{Dose} = S \tau_R \tau_G \tau_C \frac{\tau_{\text{sec}}}{DF} \frac{\chi}{Q} \cdot \text{conv.} \cdot \frac{\text{Dose rate}}{\text{air conc.}},$$

where

- S = nuclide inventory of fuel element, Ci;
- τ_R = release fraction, assume 1;
- τ_G = transfer term for graphite, assume 1;
- τ_C = leakage rate of nuclide/entry rate to coolant — at steady state,

$$\tau_C = r_\ell / (r_\ell + r_p + r_s + \lambda),$$

where

- r_ℓ = 1.2×10^{-9} /sec if primary coolant leakage is 0.01%/day;
- r_p = purification plant rate, 6.2×10^{-5} /sec;
- r_s = plateout removal by surfaces — for 1% per pass,
 $r_s \approx 0.0022$ /sec;
- λ = radioactive decay constant of the nuclide;
- DF = decontamination factor — concrete and secondary containment: use 1 for noble gases, 100 for other elements;
- τ_{sec} = secondary containment transfer term = $\frac{\text{sec purge/vol}}{\lambda + \text{sec purge/vol}}$;
 $\frac{\text{sec purge}}{\text{vol}} \approx 1.5 \times 10^{-4}$ /sec (basis Delmarva PSAR);
- $\chi/Q \approx 1 \times 10^{-3}$ (Ci/m³)/(Ci/sec).

The conversion factor conv. for submersion is (year/sec) and for inhalation, breath rate (m³/sec)/annual intake (m³/year). Dose factors are in (mrem)/year•(Ci/m³) and are understood to be the annual dose commitment corresponding to unit air concentration. The effects of daughters produced internally is understood to be included. Dose factors^{9,10} are consistent with ICRP committee 2 (1959) values.

The maximum dose resulting from the release from the particle of all the inventory of a given nuclide for one four-year-old fuel element was calculated under the above assumption, using standard inventory values³ from Table 11.1-5 of GASSAR-6 for a 3000-MW(t) HTGR. Complete

release and instantaneous passage through graphite were assumed. The effects of all nuclides of a given element were obtained and included in the doses described below.

No dose factors are available for selenium. However, its activity is appreciably less than that of tellurium. The whole-body dose at the exclusion area boundary from tellurium isotopes in a single fuel element is about 3×10^{-7} rem. The dose to the gastrointestinal tract is about 1.2×10^{-6} rem.

From bromine isotopes the maximum whole-body dose would be about 5×10^{-10} rem and that to the lower large intestine about tenfold higher.

The maximum whole-body dose from iodine isotopes is about 2×10^{-7} rem; the dose is about 1000-fold higher (2×10^{-4} rem) to the thyroid.

The maximum whole-body doses from noble gases results from submersion and amounts (for one fuel element) to about 1.4×10^{-4} rem for all the various nuclides, ^{88}Kr being the dominant contributor.

The maximum whole-body dose from the rubidium isotopes in one fuel element is about 3×10^{-10} rem; values for the gastrointestinal system are about tenfold higher.

The maximum whole-body dose from cesium isotopes in one fuel element is about 1×10^{-6} rem; the dose to the lower intestine is 75 times greater. Major contributors are ^{134}Cs , ^{136}Cs , and ^{137}Cs .

The maximum whole-body dose from strontium isotopes is about 10^{-5} rem. The dose to bone is about 20-fold higher. The whole-body dose attributable to ^{140}Ba is 7×10^{-7} rem, and the dose to the lower intestine is 20-fold greater.

The elements considered above are the gaseous and volatile elements. The dose effects of less volatile elements, including actinides, should be smaller than these because of more difficult release and transport through graphite.

In the above we have neglected the continued production of the various nuclides; this will be significant only for rather short-lived nuclides ($1/\lambda \ll 1$ hr). The continued production indicates that full

inventory will be present at the moment of release. The interaction of nuclide production by decay and the transport characteristics were not taken into account. The effects of daughter decay are understood to be included in dose values.

The effects of slow release from particles and slow passage through graphite to the coolant were ignored by setting the transfer terms equal to unity. However, release resistances are sufficient in the cases noted in the GASSAR³ (Sect. 2A.9) to reduce the fractions of initial activity released in 2 hr to 2% for noble gases, 0.1% for iodine, and essentially zero for particulates. Although this indicates a real resistance to release, the more rapid temperature increase in our case indicates higher release rates; exact evaluation is not attempted here. Instead, complete prompt release is assumed.

The loss of three associated core support posts could permit the slumping of seven columns of eight fuel elements (plus reflector elements); a total of 56 fuel elements thus could be involved.

The whole-body dose at the exclusion area boundary due to the severe overheating of 56 elements under the above conditions would amount to about 10 mrem, largely due to submersion dose in ⁸⁸Kr. Further a thyroid dose of 11 mrem is indicated. Although such doses are significant with respect to annual releases regulated by 10 CFR Part 50, Appendix I, they are far below accident release limits of 10 CFR Part 100.

1.3.2.2 Coolant Activity Increase

An increase in coolant activity might be utilized to detect overheated fuel elements if the effect were large enough and quick enough. In estimating this possibility, the criterion will be the ratio of added coolant activity to that circulating.

Only the rare gases — krypton and xenon — will move readily enough and resist plateout enough to permit accumulation sufficiently rapidly to serve as signals. Thus we wish to consider the source quantities and rates of transport of these, from previously intact particles, as the temperature of cracking is exceeded. The source is the amount of these nuclides contained in particle kernels and buffer zones.

Essentially all particles will be cracked⁷ at a fuel temperature of 2000°C. The release rate of krypton from the kernel of cracked particles at this temperature is indicated to be about 3%/min, and that of xenon is about 0.8%/min, based on charts presented in the SORS report⁸ (GA-A-12462, Fig. 5.3). Such rates, though not trivial, are not the major part of the most prompt signal.

The accumulation of radioactive rare gases in the "buffer zone" of intact particles will amount to the steady-state kernel release fraction plus the fraction released by recoil. The release fraction ($FR = R/B$) at 1100°C is 1% for a 1-hr krypton or 10-hr xenon, and is proportional to the square root of half-life (ref. 3, GASSAR, Fig. 11.1.1). An average release fraction of about 1.5% for the various krypton and xenon nuclides will be assumed.

The recoil fraction is given by $F_r = (3/4)(r/\alpha)$ where r is the average recoil range in kernel material and α the fuel kernel radius. For Triso particles with a UC_2 kernel diameter of 200 μm , values for F_r are about 6.1% for light fragments and 3.7% for heavy fragments. For Biso particles with a ThO_2 kernel diameter of 500 μm , values for F_r are about 2.8% for light fragments and 1.7% for heavy fragments. An overall average recoil fraction of 3.5% appears justifiable.

Passage through graphite involves the following considerations: For the system krypton-helium,¹¹

$$\log D_{12} = 1.688 \log T - 4.3844 .$$

Neglecting Knudsen diffusion,

$$D_{eff} \approx (\epsilon'/q)D_{12}/P ,$$

where

ϵ' = free space available for diffusion/volume of solid,

q = tortuosity or $\frac{\text{average path length through solid}}{\text{thickness of solid}}$,

P = pressure in atmospheres.

Thus at 2000°C and 50 atm, for a typical graphite (H-327, used in the Fort St. Vrain Reactor) where $(\epsilon'/q) \sim 4 \times 10^{-3}$,

$$D_{eff} \approx 1.5 \times 10^{-3} .$$

The release fraction F of a limited well-mixed source through a slab barrier is given^{12,13} by a function relating F and θ .

F	0.01	0.10	0.50	0.90
θ	0.055	0.128	0.390	1.020

where

$$\theta = D_{\text{eff}} t / \ell^2 \epsilon .$$

Assume a porosity $\epsilon = 0.20$. For a graphite web of about 0.5 cm thickness (ℓ),

$$t_{50\%} = \theta_{50} \frac{\ell^2 \epsilon}{D_{\text{eff}}} = \frac{0.39 \times (0.5)^2 \times 0.20}{1.5 \times 10^{-3}} = 13 \text{ sec} .$$

About 10% would emerge within about 4 sec after release, and 90% would have emerged in 35 sec. The rate for xenon would be somewhat slower. Steadily increasing temperature and a modest pressure gradient would speed up the process to some extent.

For a 3150-MW(t) reactor the equilibrium inventories of particular nuclides are given in Table 1.1.

Table 1.1. Equilibrium Inventories of Noble-Gas Fission Products in a 3150-MW(t) Reactor

Krypton Isotope	Half-Life	Activity (Ci)	Xenon Isotope	Half-Life	Activity (Ci)
83m	1.85 h	2.54×10^7	131m	11.96 d	5.42×10^5
85m	4.4 h	2.81×10^7	133m	2.26 d	5.05×10^6
85	10.76 y	1.97×10^6	133	5.27 d	1.81×10^8
87	76 m	8.27×10^7	135m	15.7 m	2.81×10^7
88	2.79 h	1.47×10^8	135	9.16 h	2.09×10^7
89	3.18 m	1.35×10^8	137	3.82 m	1.59×10^8
90	32.3 s	1.29×10^8	138	14.2 m	1.67×10^8
Total		5.79×10^8	139	40 s	1.33×10^8
			140	13.6 s	1.03×10^8
			Total		6.36×10^8

The inventory activity of rare gases is about 1.22×10^9 Ci. For half-lives longer than 1 min, the inventory is about 8.5×10^8 Ci.

The activity that might be available could be estimated as a function of time from the above considerations. As a first approximation, possibly sufficient for present considerations, the following assumptions will be used:

1. all nuclides of half-life below 1 min and decay of others are neglected;
2. 56 out of 3944 fuel elements are affected;
3. an average recoil fraction of 3.5% and an additional release of 1.5% by prior kernel release are assumed; subsequent kernel release (rate about 2%/min) is not used;
4. krypton diffusion properties discussed above are used.

The amount of rare gas activity thus entering the coolant in 13 sec, half the total promptly available, is then estimated to be

$$8.5 \times 10^8 \times \frac{56}{3944} \times 0.05 \times 0.5 = 3 \times 10^5 \text{ Ci} .$$

For a 3150-MW(t) HTGR (GASSAR),³ the total activity in the coolant under "design conditions" is 2.1×10^5 Ci (about 1.5×10^4 Ci under "expected" conditions). Thus if all activity were equally detectable, in about 13 sec a measurement of coolant activity should be at least double the "design" value.

Within 1 min after the fuel particle coatings reach 2000°C, the indicated coolant activity would at least have tripled its value if the reactor were operating under "design conditions." The above ratio change would be about 15-fold under "expected" conditions. Similar but generally smaller contributions would come in somewhat more slowly, from Te, I, Rb, Cs, and other elements; these also could be expected to plate out rapidly and thus accumulate less in the coolant.

Coolant activity would thus more than double before an additional rise of 100°C occurred. Such a response would appear to be a useful signal of the accident under consideration.

1.4 STEAM GENERATOR REVIEW — Richard C. Gwaltney

Reviewing the design and structural performance of the HTGR Steam Generator began in December 1974. General Atomic proprietary documents, along with open-literature reports, were reviewed for information on the design. However, during this period, the steam generator was being redesigned by GA, and an early effort was directed toward examining the materials and design philosophy used in the design of the steam generator.

One of the most distinguishing features of high-temperature design methods is that inelastic material behavior must be taken in account in determining the design life of a component. Both time-independent elastic-plastic behavior and time-dependent creep behavior must be considered. These individual behaviors, as well as the total combined inelastic response for a given material, are very complex, consisting of processes and interactions that are not completely understood and that currently defy exact description. To carry out rational design analyses and assessment, essential significant features of the behavior of a material must be identified and understood. This is usually done by developing constitutive equations, which are mathematic statements of the phenomenological model of the mechanical behavior of a material.

For detailed inelastic analyses, constitutive equations for each material to be used must be available. Analyses by either detailed or simplified methods for a high-temperature environment require describing the response of the material to time-varying load and temperature histories.

The major materials¹⁴⁻¹⁷ used in the design of the steam generator are listed in Table 1.2 with projected maximum operating temperatures. The three materials being used in high-temperature environments are (1) austenitic type 304 stainless steel, (2) 2 1/4 Cr-1 Mo steel, and (3) Incoloy alloy 800H. The necessary material data, material correlations, and mathematical models that are ideally required for inelastic analyses for these three materials are tabulated in Table 1.3 with the data sources that we are aware of. The temperature range for which the data exist is also tabulated.

Table 1.2. Steam Generator Materials and Projected Operating Temperatures

Component	Material	Maximum Temperature	
		(°C)	(°F)
Reheater			
Tube bundle	Incoloy 800H	760	1400
Lead-out tubes	Carbon steel	371	700
Lead-in tubes	Incoloy 800H	649	1200 (mean)
Tube support plate	Incoloy 800H	816/704	1500/1350
Reheater structure	Incoloy 800H	816/704	1500/1350
Tubesheet (hot)	Incoloy 800H	627/538	1160/1000
Tubesheet (cold)	Carbon steel	338	640
Shroud and crossover duct	Incoloy 800H	854/732	1570/1350
Superheater III			
Tube bundle	Incoloy 800H	649	1200 (mean)
Tubesheet	Incoloy 800H	593/510	1100/950
Shroud	Type 304 stainless steel	760	1400
Tube support plate	Incoloy 800H	760	1400
Superheater II			
Tube bundle	Incoloy 800H	649	1200 (mean)
Tube support ladder	Incoloy 800H	724	1335
Shroud (inner)	Incoloy 800H	738	1360
Shroud (outer)	Incoloy 800H		
Economizer-Evaporator			
Superheater I			
Tube bundle			
Superheater I	2 1/4 Cr-1 Mo steel	538	1000 (mean)
Evaporator II	2 1/4 Cr-1 Mo steel	482	900 (mean)
Evaporator I	2 1/4 Cr-1 Mo steel	454	850 (mean)
Economizer	2 1/4 Cr-1 Mo steel	371	700 (mean)
Economizer lead-in tube support plate	Incoloy 800H	704	1300
Tubesheet	Carbon steel	260	500
Steam generator support flange	2 1/4 Cr-1 Mo steel	371	700

Table 1.3. Material Property Data for High-Temperature Applications in HTGR Steam Generators

Material Property Data	2 1/4 Cr-1 Mo			Incoloy 800H			Type 304 Stainless Steel		
	Temperature Range		Source	Temperature Range		Source	Temperature Range		Source
	(°C)	(°F)		(°C)	(°F)		(°C)	(°F)	
Creep data	371-538	700-1000	a				427-701	800-1300	b
Thermal expansion	20-700	68-1292	b	21-871	70-1600	c	24-816	75-1500	b
	21-649	70-1200	c	21-982	70-1800	d	21-816	70-1500	c
Elastic modulus	-18-649	0-1200	b	21-871	70-1600	c	24-816	75-1500	b
	21-649	70-1200	c	21-982	70-1800	d	21-816	70-1500	c
				427-760	800-1400	e			
Poisson's ratio	-18-649	0-1200	b	427-760	800-1400	e	24-816	75-1500	b
Yield stress	21-593	70-1100	b	21-871	70-1600	c	24-816	75-1500	b
	21-649	70-1200	c				38-816	100-1500	c
Constitutive equations ^f	24-538	75-1000	g				24-649	75-1200	h
Cyclic stress-strain curves	21-593	70-1100	i,j				427-649	800-1200	b,k
							427-816	800-1500	e
Volumetric heat capacity	24-700	75-1292	b				24-816	75-1500	b
Thermal conductivity	300-700	572-1292	b				24-816	75-1500	b
Creep rupture (stress to rupture)	371-593	700-1100	a	454-760	850-1400	c	427-816	800-1500	c
	399-649	750-1200	c	760-1093	1400-2000	d			
Fatigue strain range	371-593	700-1100	a	427-760	800-1400	c	38-704	100-1300	c
				427-760	800-1400	e	427-816	800-1500	e
Creep fatigue				427-649	800-1200	c,l	427-649	800-1200	c,l
							427-649	800-1200	m
Isochronous stress-strain curves	371-649	700-1200	c	454-760	850-1400	c	427-816	800-1500	c

^aMaterial to be included in *Nuclear Systems Materials Handbook*.

^b*Nuclear Systems Materials Handbook*, TID-26666.

^cASME Code Case 1592.

^d*Structural Alloys Handbook*, 1976 ed., Mechanical Properties Data Center, Belfour Stulen, Inc., Traverse City, Michigan.

^eJ. B. Conway, R. H. Stentz, and J. T. Berling, *Fatigue, Tensile, and Relaxation Behavior of Stainless Steels*, TID-26135 (1975).

^fMathematical expressions or procedures used to represent phenomenological models for material behavior. This behavior is based on most of the material properties listed in this table.

^gC. E. Pugh et al., *Background Information for Interim Methods of Inelastic Analysis for High-Temperature Reactor Components of 2 1/4 Cr-1 Mo Steel*, ORNL/TM-5226 (May 1976).

^h*Guidelines and Procedures for Design of Nuclear System Components at Elevated Temperature*, ERDA RDT Standard F9-5T, September 1974.

ⁱC. E. Jaske, B. N. Leis, and C. E. Pugh, "Monotonic and Cyclic Stress-Strain Response of Annealed 2 1/4 Cr-1 Mo Steel," pp. 191-212 in *Structural Materials for Service at Elevated Temperatures in Nuclear Power Generation*, MPC-1, American Society of Mechanical Engineers, New York, 1975.

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^kFor certain heats of material.

^lIncluded in Fig. T-1430 of Code Case 1592 for use with simplified elastic methods.

^mR. W. Weeks, D. R. Diercks, and C. F. Cheng, *ANL Low-Cycle Fatigue Studies - Programs, Results, and Analysis*, ANL-8009 (November 1973).

As shown in Table 1.3, enough data for inelastic analyses are available for type 304 stainless steel and 2 1/4 Cr-1 Mo steel. Creep data and mechanical properties data for type 304 stainless steel are being compiled and developed under the ORNL program on mechanical properties of LMFBR structural materials¹⁸ and for 2 1/4 Cr-1 Mo steel under the ORNL Steam Generator Materials Technology Program.¹⁹ The constitutive equations for type 304 stainless steel^{20,21} and 2 1/4 Cr-1 Mo steel²² were developed under the ORNL High-Temperature Structural Design Program. Most of the data for these two steels are available in standard references.^{21,23-25} Under the ORNL High-Temperature Structural Design Program, the validation²⁶ of the constitutive equations for type 304 stainless steel by high-temperature inelastic structural tests of simple structural models, such as simply supported beams and circular plates, is well under way, and future tests will be done on a model with a radial nozzle attached to a spherical shell. The validation for 2 1/4 Cr-1 Mo steel has begun with tests on simply supported beams.

General Atomic has developed its own cyclic hardening curves for Incoloy 800H. These curves are based on different assumptions²⁷ than those recommended in refs. 20-22. General Atomic uses these hardening curves in the recommended procedures for stainless steel.^{20,21} The use of their own cyclic hardening curves in the recommended procedures constitutes General Atomic's constitutive equations for Incoloy 800H.

As Table 1.3 shows, Incoloy alloy 800H data are not generally available in several key areas. This lack of creep data, cyclic stress-strain curves, and validated constitutive equations is a very serious shortcoming. Incoloy 800H constitutive equations should be validated before any reliance is placed on their use in inelastic analysis calculations. This verification can best be carried out by inelastically analyzing simple structural models that have been experimentally tested at elevated temperatures.

During this reporting period, a report on an interim assessment of the structural performance of the steam generator was begun. Because

of the redesign of the steam generator, this report will be a preliminary assesment of the structural design limited to a review of materials information and design philosophy.

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2. FISSION PRODUCT TECHNOLOGY

E. L. Compere

2.1 ACCIDENT HAZARD POTENTIAL OF KEY NUCLIDES

The key nuclides ^3H , ^{88}Kr , ^{90}Sr , $^{127\text{m}}\text{Te}$, ^{131}I , and ^{137}Cs were taken by de Nordwall and Bell¹ to represent chemically and radiologically all significant mobile fission products in HTGR systems. They suggested that the effects of routine and accidental releases could be examined in terms of these nuclides.

The secondary containment is the last barrier between the plant and the public. Plant releases corresponding to unit dose that could be accumulated by members of the public at the exclusion area boundary have been derived from values of dose rate/air concentration and an assumed meteorological dispersion factor (χ/Q) under worst-case atmospheric assumptions. The dose unit for a given key nuclide is taken as 1/25 of the 10 CFR Part 100 limit, thus is 1 rem, except for ^{131}I , where the thyroid dose unit is taken as 12 rem. The derived plant release value limits corresponding to such unit dose are shown in Table 2.1.

The amounts of the respective key nuclides in various locations possibly accessible to release will be examined, and the adequacy of the retention parameters of the various barriers for various accidents considered. Among the locations within the primary containment are the fuel, the fuel block and other graphite, the coolant gas itself, and system surfaces (including dust) on which volatile metal fission products may deposit as "plateout." The importance of the various locations varies from one nuclide to the other.

Tritium — The derived plant release unit is 26,000 Ci for 1 rem whole-body dose by inhalation. Only the release of the entire reactor inventory of 23,000 Ci, largely contained within coated particles, would enable this value to be approached. Such release would require very severe core overheating and negligible transport resistance by both primary and secondary containments. Only maximum hypothetical accidents would include such conditions, and in such case the contribution

Table 2.1. Hazard Potential of Key Nuclides — Design Basis: 2000-MW(t) HTGR,
80% Service, 2.7% Cracked Particles, 40% Plateout per Pass,
 $\chi/Q = 1 \times 10^{-3} \text{ (Ci/m}^3\text{)}/\text{(Ci/sec)}$

Nuclide	Half-Life	Inventory, Ci				Derived Release for Given Dose at Exclusion Area Boundary				
		Total	Design Basis, Outside Fuel			Plant Release (Ci)	Dose (rem)	Organ	Mode	Factor ^a (rem/sec)/(Ci/m ³)
			Graphite	Plateout	Gas					
³ H	12.3 y	2.3×10^4	1000-5000 ^b	0	2.7	26,000	1	Whole body	Inhalation	0.038
⁸⁸ Kr	2.79 h	9.8×10^7	0	0	23,000	2,000	1	Whole body	Submersion	0.40
^{127m} Te	109 d	1.73×10^6	?	7,500	0.2	1,700	1	Whole body	Inhalation	0.57
¹³¹ I	8.06 d	7.03×10^7	Significant ?	237,000	28	1,100	1	Whole body	Inhalation	0.91
						24	12	Thyroid	Inhalation	510
¹³⁷ Cs	30.2 y	5.22×10^6	Significant	107,000	0.06	90	1	Whole body	Inhalation	11
⁹⁰ Sr	28.9 y	5.39×10^6	Significant	110,000	0.005	2	1	Whole body	Inhalation	450
						0.15	1	Bone		6800

^aDose commitment based on inhalation rate 10 m³/8 hr or on submersion.

^b150 Ci per part per billion lithium from ⁶Li(n,α)³H; 50% of 1500 Ci/year at ³He abundance of 2×10^{-7} from ³He(n,p)³H; 300 Ci/year from ¹⁰B(n,2α)³H; balance from fuel.

of tritium to accident effects would be completely overshadowed by those of other nuclides. Although release of tritium during operation within technical specifications must be considered carefully, it may be ignored in considering the consequences of postulated major accidents.

Krypton — A possible dose of 1 rem by submersion at the exclusion area boundary could result from plant release of 2000 Ci ^{88}Kr . The primary coolant gas under "design" conditions could contain 23,000 Ci. However, the design secondary containment release rate of 0.25%/day (0.0002/2 hr) is more than adequate even for an accident such as a DBDA (design basis depressurization accident), in which the main part of the primary coolant is released into the secondary coolant.

If severe overheating of the entire core were postulated, as in certain hypothetical accidents, so that the entire inventory (9.8×10^7 Ci ^{88}Kr) could be at risk, then it would become necessary to consider various factors affecting retention and transport of fission gases, in order to determine whether adequate margins of safety can exist.

Tellurium — A whole-body dose of 1 rem could occur by inhalation at the exclusion area boundary as a result of a plant release of 1700 Ci $^{127\text{m}}\text{Te}$. However, little $^{127\text{m}}\text{Te}$ is gasborne (0.2 Ci). Plate-out inventory, estimated under design conditions to be 7500 Ci, is sufficiently low that a plant release of 1700 Ci would not be anticipated unless secondary and primary containments were largely ineffective, and further that additional events resulting in lift-off of most of the tellurium occurred. Heating of surfaces and attack by air and steam should be considered in this respect.

The release of the entire inventory of 1.7×10^6 Ci $^{127\text{m}}\text{Te}$ (possible only if all coated particles fail) into the secondary containment (designed to release only a fraction 0.0002/2 hr) should not result in exceeding the derived plant release limit.

Iodine — The plant release of 24 Ci ^{131}I could result in a thyroid dose of 12 rem. Although the coolant gas could contain 28 Ci under design conditions, it is doubtful that a sufficient fraction of this could be transported to result in a significant

plant release relative to the derived limit. However, the plateout of 237,000 Ci is large enough so that other transport resistance factors in addition to retention by secondary containment would be needed. Isothermal partition factors between gas and primary system surfaces are low enough to provide useful retention. However, the effects of heating of the surfaces or the attack of surfaces by moisture or air should be accounted for, since these could result in increased release of iodine from surfaces.

The amount of iodine held by graphite at the beginning of an accident could contribute significantly to the source term and should be taken into account.

Evidently any accidents in which major fractions of the coated fuel particles could fail could place the entire inventory of 6×10^7 Ci ^{131}I at risk, and transport resistance and retention factors in addition to leakage rates from secondary containment will require careful consideration. In particular the effects of core overheating and of steam or air ingress coupled with violation of primary containment should not be neglected.

Cesium — A whole-body dose of 1 rem to an individual at the exclusion area boundary could result from plant release of 90 Ci ^{137}Cs . Effective secondary containment is approximately sufficient for adequate retention of all cesium plateout, even though cesium adsorption in some regions might have approached saturation, with consequently reduced retention. The appreciable amount of cesium held in fuel and reflector graphite is poorly known but would likely be released during core heatup before temperatures leading to failure of particle coatings are reached. Release of the full reactor inventory into the secondary circuit requires particle failure and breach of primary containment, and consideration should be included in scenarios in which this might occur.

Strontium — A whole-body dose of 1 rem (or bone dose of ~15 rem) could result from a plant release of 2.0 Ci ^{90}Sr . Thus like for cesium, plateout, graphite content, and full core inventories are important. The lower volatility and reduced transport (relative to cesium) are insufficient to offset the low release limit, so that the

hazard potential of strontium would appear to be 3 to 4 times that of cesium. In the case of strontium as with other isotopes, loss of retention in primary containment, coupled with other loss of retention at the source location as a result of severe heating or chemical attack, appears required for serious hazards to develop.

Summary — Iodine, cesium, and strontium require careful consideration in scenarios where primary containment is breached and factors causing loss of retention at the source location are present. Otherwise the secondary containment, performing as designed, can be expected to be sufficient to keep releases low enough that doses will not exceed the regulatory limit.

2.2 IODINE ADSORPTIONS AND DESORPTION

Data for the chemisorption of iodine on Fe_3O_4 surfaces were reported previously.² The data may be summarized as isotherms of the Temkin form:

$$\ln p = -C + \ln[\theta/(1 - \theta)] + B\theta ,$$

where p is the equivalent iodine atom pressure in atmospheres, θ is the occupied fraction of 2×10^{14} sites/cm² and the constants B and C have the values at given temperatures as follows:

°C	B	C
200–300	6.26	23.71
350	5.90	22.12
400	7.81	22.06
450	9.36	22.49

This expression will assist in defining any calculated source term for the primary coolant system and the equilibrium partition factor for desorption under appropriate circumstances.

To estimate the limits on the release of chemisorbed iodine during a DBDA (design-basis depressurization accident), we shall assume

a simple model in which the gas and a single surface remain isothermal and in equilibrium as the coolant gas is released. For 350°C and 20% coverage, an iodine partial pressure of 2×10^{-10} atm (20 μ Pa) is obtained from the above expression.

A partition ratio, Z , (atoms/liter)/(atoms/cm²) is obtained:

$$Z = \frac{6.02 \times 10^{23}}{0.08206 TS^\circ} \cdot e^{-C} \frac{e^{B\theta}}{1 - \theta} .$$

At 350°C, $\theta = 0.2$; also, $S^\circ = 2 \times 10^{14}$ sites/cm², whereby $Z = 0.059$.

For an isothermal release of gas from pressure P° to P with equilibrium maintained at all times between gas and surface,

$$\left(1 + \frac{A}{VZ}\right) \ln \frac{N^\circ}{N} = \frac{P^\circ}{P} ,$$

where A is the system surface area, V is the effective gas volume, and N° and N are the combined gas and surface content of the given substance (here iodine) initially and finally.

The effective volume would be that occupied by the coolant (4227 kg He) at 47.6 atm (4.82 MPa) and 350°C, which is 1.14×10^6 liters. If we assume a surface of 2×10^8 cm² available for iodine adsorption, we find

$$1 + \frac{2 \times 10^8}{1.14 \times 10^6 \times 0.59} \ln \frac{N^\circ}{N} = \ln \frac{47.6}{1} ,$$

whereby $N^\circ/N = 1.0013$ and the fraction of release is 0.0013.

For a 2000-MW(t) HTGR³ the "design" plateout of ¹³¹I is 2.37×10^5 Ci and the "design" gas content is 80.3 Ci, whereby the maximum desorption release to the secondary containment would be about 308 Ci.

Hazard analysis indicates release from primary containment would result in a dose estimated as follows:

$$\text{Dose, rem} = R \times \tau_{\text{sec}} \times \frac{X}{Q} \times \text{conv.} \times \frac{\text{dose rate}}{\text{air conc.}} ,$$

where

$$\begin{aligned}
 R &= \text{release from primary, Ci;} \\
 \tau_{\text{sec}} &= \text{fraction of amount released entering secondary;} \\
 \chi/Q &= \text{atmospheric dispersion; use } 1 \times 10^{-3} \text{ (Ci/m}^3\text{)/(Ci/sec).} \\
 \text{conv.} &= \frac{\text{Breath rate per sec}}{\text{intake per year}} : \text{ Use } \frac{30 \text{ m}^3/86400 \text{ sec}}{7300 \text{ m}^3/\text{year}} .
 \end{aligned}$$

$$\frac{\text{dose rate}}{\text{air conc.}} = 1.07 \times 10^{10} \frac{\text{rem/year}}{\text{Ci/m}^3} \text{ for thyroid dose, } ^{131}\text{I} \text{ inhalation.}$$

Thus, for a DBDA release of 308 Ci,

$$\text{Dose} = 308 \times 0.002 \times 1 \times 10^{-3} \times \frac{30}{86400 \times 7300} \times 1.07 \times 10^{10} = 0.31 \text{ rem.}$$

It is clear that provided the assumed secondary containment release fraction is correct, isothermal desorption of iodine from primary circuit surfaces in a DBDA would not result in excessive dose.

2.3 ASSESSMENT OF CESIUM TRANSPORT PARAMETERS

The hazard potential of individual fission product nuclides depends on the source inventories in relevant locations and on the possibility of transport during accident situations. The source inventories are derived from material transport and capacity properties that exist during normal operation. Consistent values of the needed parameters are not always available in the literature.

Graphite — For ^{137}Cs , Sect. 2.1 showed that a plant release of 90 Ci in the course of an accident could result in a dose commitment of 1 rem to an individual at the exclusion area boundary. At least this much activity must enter or be in the primary coolant of an HTGR if such a consequence is to be possible, since the only evident mode of transport from the various source locations is by movement of the gas.

For the 2000-MW(t) Summit (Delmarva) HTGR, values for ^{137}Cs were given in the PSAR⁴ as follows:

Gasborne	0.06 Ci	"Design" Basis
40-year plateout	1.07×10^5 Ci	"Design" Basis

The ^{137}Cs inventory of the equilibrium reactor (four-year cycle) has been estimated to be about 5.2×10^6 Ci. Evidently the gasborne inventory of ^{137}Cs of itself is not of major consequence, but possibilities of transporting either plateout material or other fractions of the reactor inventory into the primary coolant require serious consideration.

In passing from fuel particles to the primary coolant channels during operation, fission product atoms must migrate through both the fuel rod matrix carbon and the structural graphite of the fuel element. Both of these can adsorb cesium to a considerable extent. Migration rates, normally expressed in terms of the diffusion coefficient as in a homogeneous substance, are low, resulting in considerable retention of cesium in these solids. The release was calculated by use of an appropriate version of the FIPER^{5,6} code. In addition to dimensional characteristics and temperature distribution, the diffusion coefficients and matrix and graphite sorption properties are required. For the cesium-graphite system, the Summit PSAR gave:

$$\ln D = -1.0 - 39,000/RT ,$$

where D is the Fickian diffusion coefficient (cm^2/sec) of cesium in graphite, T the absolute temperature (K), and R the gas constant (1.987 cal/mol K). (Editor's note: 1 cal = 4.184 J.)

The vapor pressure of cesium adsorbed on graphite was given by a Freundlich isotherm,

$$\ln P_M = 28.4 - 56,000/T + (-19.0 + 31,700/T) \ln C_M ,$$

where P_M is the equilibrium pressure of cesium (atm, 1 atm = 0.10132 MPa), and C_M the cesium concentration on graphite internal surfaces, ($\mu\text{mol}/\text{m}^2$).

Matrix (filler) material was indicated to sorb about 500 times as much cesium, on a weight basis, as fuel element graphite. The diffusion coefficient in the fuel rod was assumed to be at least an order of magnitude larger than the diffusion coefficient in the structural graphite.

Use of the above indicates that the diffusion resistance of the graphite can cause the ^{137}Cs released from fuel particles over a period of about a year or more to be retained on the matrix carbon and in the graphite. This effect was included in the cited estimate of the quantity of plateout and indicates an appreciable source of ^{137}Cs to be adsorbed on matrix carbon and graphite, subject to transport by increase in temperature.

The SORSG code⁷ considers the release of metallic fission products as a result of major temperature changes in accident situations. However, entirely different values for diffusion and sorption parameters are given. The diffusion coefficient of cesium in graphite is given, in terms of variables defined above, by

$$\ln D = -0.48 - 18,760/RT .$$

This results in values of D several orders of magnitude greater than that cited in the Summit PSAR.

The adsorption of cesium on graphite was given, in terms of variables defined earlier, as:

$$\ln P_M = 7.8 - 29,840/T + (0.2351 + 1873/T) \ln C_M .$$

This expression is notably different from that cited earlier.

The use of these latter expressions for diffusion and sorption result in much shorter time factors, of the order of days, with the indicated of higher plateout, less retention by graphite, and more rapid release from graphite though with diminished temperature sensitivity.

Plateout -- The safety margin for cesium and possibly other fission product elements may be lower than indicated by calculations summarized in PSARs. The "design plateout" is understood to be obtained from the RAD code,⁸ with "design" parameters, and a source term that is presumed to be based on FIPER^{5,6} calculation. A "design" plateout rate of 1% per pass is assumed, in conjunction with a purification plant removal rate of the order of 4 to 6 per day. Adsorption is treated as an

irreversible sink. The 40 year plateout value for a 3000-MW(t) HTGR is thus estimated to average about 150 monolayers of cesium over all possible plateout surface.

The assumptions stated above are not conservative in all aspects. Only a few sets of adsorption isotherms for cesium on steels have been reported.^{9,10} These generally show that adsorption on essentially unoxidized metal ceases after only a few monolayers are present and is fairly independent of temperature or pressure. Oxidation of stainless steel surfaces considerably increases the amount adsorbed, with limiting values up to about 80 to 90 monolayers observed, fairly independently of temperature. However, at higher temperatures Freundlich pressure isotherms are obtained for adsorption on oxidized stainless steel. Thus the extent of surface oxidation appears to be an essential but ill-defined parameter.

Treatment of adsorption by mass transfer codes, such as PAD, indicates that initially mass transfer will limit the deposition rate on all surfaces. In the course of time surface saturation will develop in some region — frequently upstream — and deposition becomes isotherm-limited there. The isotherm-limited region will increase, and nuclide partial pressures rise, until ultimately the entire surface could be isotherm controlled, and deposition would cease, although exchange processes, important for short-lived nuclides, could continue. At this point the gas pressure would be controlled by the inlet flux and by the removal rate of the purification plant (and by decay processes). In the case of ^{137}Cs in particular, the partial pressure could increase by about 50-fold or more over that for 1% per pass.

Under such saturation conditions, the increased cesium partial pressures in the gas phase will increase the activity available for immediate transport in case of accident. Saturation will also imply that the surfaces can readily release adsorbed material to maintain partial pressures as total pressure is decreased (as in a DBDA). Such partial pressure maintenance indicates that increased hazard associated with the increased content of cesium isotope in the circulating gas is augmented appreciably by release from surfaces during a postulated blowdown.

Consequently the following points are indicated:

1. Isotherm control of fission product plateout under design conditions must be taken into account in the estimate of inventories for plateout and gas.
2. The release from saturated surfaces during the course of any postulated accident must be taken into account.
3. Evidently an increase in surface temperature in the course of a postulated accident may become highly significant, as reported isotherms vary appreciably with temperature.
4. Design-basis assumptions as to oxidation of various surfaces must be established.
5. Isotherms must be established for such oxidized surfaces.
6. Increased partial pressures under conditions of isotherm control will also increase adsorption by any dust, and thus augment the associated hazard potential.

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3. PRIMARY COOLANT TECHNOLOGY
EFFECT OF STEAM CORROSION ON CORE POST STRENGTH LOSS:
CASE OF LOW-LEVEL LONG-TERM INLEAKAGE

R. P. Wichner

3.1 OBJECTIVE AND METHOD

We assessed the effect of chronic low levels of steam ingress into the primary system of an HTGR on the corrosion and consequent strength loss of the core support posts. To arrive at a judgment, the following three questions had to be addressed in sequence: (1) The impurity composition in primary coolant, which depends primarily on the core graphite reactivity to steam corrosion, had to be estimated for a range of assumed steam ingress rates. The composition of impurities determines the oxidizing environment experienced by the core post. (2) Burnoffs that result from 40-year exposure to these determined impurity levels were then determined by employment of an estimated corrosion susceptibility of core post graphite. (3) The final step in the assessment involves estimation of the degree of strength loss resulting from these estimated burnoffs. An empirical equation was developed for this purpose and compares reasonably well with strength loss data for a number of different graphites.

3.2 PREDICTED COOLANT IMPURITY COMPOSITIONS

A series of impurity addition experiments was performed¹ at the Dragon Reactor. It included a sequence of four H₂O additions, at rates of 2 to 3 g/hr, extending over approximately 30 hr. Table 3.1 summarizes the results of these Dragon tests. These data may be interpreted to yield an effective reactivity to steam oxidation of the Dragon core graphite defined by the simple relation,

$$\text{Rate} \left(\frac{\text{mole}}{\text{sec}} \right) = K_c [\text{H}_2\text{O}] , \quad (1)$$

where [H₂O] represents the average concentration of steam in the primary coolant (mole/cm³) and K_c (cm³/sec) is the defined core reactivity.

Table 3.1. Steady-State Impurity Levels Established During Dragon Steam Ingress Experiments, and Normal Impurity Levels with Zero Ingress^a

	H ₂ O Injection Rate (mole/hr)	Purification Flow (g/sec)	Equilibrium Concentrations, vpm ^b						
			H ₂ O	H ₂	CO	CO ₂	CH ₄	Σ[H]	Σ[O]
1	0.153	9.0	6.0	6.3	2.37	0.91	0.38	26.1	10.2
2	0.172	11.0	3.2	7.6	3.5	1.3	0.21	22.4	9.3
3	0.172	7.5	4.2	13.1	7.0	2.1	0.45	36.4	15.4
4	0.122	7.0	3.6	7.3	5.7	1.5	0.35	23.2	12.3
-	0	(nominal)	~0.075	~1.4	~0.75	~0.02	~0.15		

^aBased on M. Carlyle and D. V. Kinsey, D.P. Rep. 544 (1969).

^bParts per million by volume.

A more complex assumed relationship involving more than one empirical constant is not warranted in this case because only four data points are involved.

The results of the Dragon ingress experiments were analyzed to determine a value for the effective reactivity to steam corrosion appropriate for the Dragon reactor. A value for K_c of $8610 \pm 2100 \text{ cm}^3/\text{sec}$ is obtained, subject to certain uncertainties. The principal uncertainty relates to the lack of a proper material balance in these experiments; about 40% of the added moisture was unaccounted for in the gaseous samples. The value for core reactivity quoted above is based on the assumption that this amount of "lost" moisture became permanently adsorbed somewhere in the primary system, and thus played no role in graphite corrosion.

This estimated value of the Dragon core reactivity to steam corrosion may be used to obtain a corresponding value for an HTGR core by noting that approximately

$$K_c \propto A_c \exp\left(\frac{-\Delta H}{RT_c}\right) \cdot \frac{1}{\sqrt{P}}, \quad (2)$$

where A_c is the exposed surface area of the core, ΔH is the activation energy, T_c represents the average graphite temperature effective in the

corrosion reaction, and P is the total coolant pressure. By comparing Dragon and HTGR design conditions and core configurations a value of $K_c = 50,600 \pm 12,300 \text{ cm}^3/\text{sec}$ is obtained for a 2000-MW(t) reference HTGR. This leads to the projected coolant impurity compositions as a function of steam ingress rate shown in Fig. 3.1, which shows that from 60 to 70% of the moisture leaking into the primary system of an HTGR is anticipated to exist as H_2O when initial transients have leveled out.

Fig. 3.1. Impurity Levels vs H_2O Ingress Rate Based on Dragon Experiments.

An alternative estimate of the impurity composition in the coolant, which results from an assumed rate of steam ingress, was obtained by taking a range of values for the core graphite's reactivity given by three published kinetics equations. (The specific rate equation for the reference HTGR moderator graphite is not available at this time.) A computer program, termed TIMOX, was used to calculate the impurity

levels in primary coolant employing these estimates of core reactivity. Some results are shown in Fig. 3.2. Note that TIMOX predicts that a smaller fraction of inleaked H_2O remains as H_2O at the lower assumed steam ingress rates than for higher ingress rates, as opposed to a constant fraction illustrated in Fig. 3.1. The reason for this behavior is that analysis of the Dragon data required the assumption that the corrosion reaction was first order with respect to H_2O , whereas TIMOX can incorporate the nonlinearities of the corrosion kinetics equation. A comparison with Dragon data projections shows that TIMOX predicts from 30 to 60% of the total oxygen to remain as H_2O at the lowest assumed ingress rate of 0.001 g/sec. The range raises to from 50 to 80% for an ingress rate of 0.01 g/sec. At higher ingress rates, the impurity composition tends to become predominantly H_2O .

Fig. 3.2. Equilibrium H_2O Concentration vs Steam Inleakage Rate Calculated from Kinetics Equations.

3.3 GRAPHITE STRENGTH LOSS DUE TO STEAM CORROSION

There is presently no fundamentally based theory to relate graphite loss of strength to degree of corrosion. In the absence of any theoretical guidelines, we adopted an empirical correlation suggested by strength loss data presented by Helsby and Everett² of the form

$$\text{FSL} = P(T) \cdot \frac{BO}{\rho_{bo}} \cdot \frac{1}{W}, \quad (3)$$

where FSL stands for fractional strength loss as a consequence of burnoff BO (g/cm^2) (degree of corrosion). The term ρ_{bo} is the original graphite density less the density of material that remains after complete burnoff, and W is the specimen width. The multiplier $P(T)$ is an empirical factor, which decreases with increasing temperature. The factors, $BO/\rho_{bo}W$, signify the fractional depth of corrosion assuming it to be entirely drawn from the surface of the member. The factor $P(T)$ amplifies this hypothetical "onionskin" corrosion depth to account for the fact that the corrosive effect runs significantly deeper into the graphite. Data show² that the effective corrosion depth, and hence the factor $P(T)$, increases with decreasing temperature for a given total degree of corrosion.

Calculated penetration factors range from 1.8 at 1140°C to 4.4 at 950°C employing the data of ref. 2. Extrapolating down to the core post temperature range involves considerable uncertainty. However, values of $P(T)$ in the range 6.0 to 14 are obtained by extrapolation, where the range reflects the uncertainty of the extrapolation. A summary of estimated penetration factors, $P(T)$, to be used in conjunction with Eq. (3) are listed in Table 3.2.

Figure 3.3 compares the strength loss correlation with data on the accelerated corrosion of H327 graphite at 900°C . The comparison between the data and the correlation is deemed reasonably good since sample geometry, graphite type, and temperature all differed from the experimental conditions² on which the correlation is based.

Table 3.2. Burnoff Penetration Factors as a Function of Temperature

Temperature (°C)	Penetration Factor $P(T)$
1140	1.8
1030	2.3
950	4.4
900	4.5–6.0 ^a
874	4.8–7.1 ^a
786	6.0–14 ^a

^aEstimated range by extrapolation.

Fig. 3.3. Changes in Strength of 13-mm-diam (0.5-in.) Samples of H327 Graphite After Accelerated Steam Oxidation at 900°C.

3.4 PREDICTED CORE POST BURNOFFS AND STRENGTH LOSS AT END OF REACTOR LIFE

The predicted core post burnoffs from 40 years of continuous steam ingress are shown in Fig. 3.4 for the core post at the assumed nominal coolant exit temperature with nominal purification flow assumed. The burnoffs, expressed in mg/cm^2 , were computed from

$$R = \frac{85.1 \times 10^6 \cdot P_{\text{H}_2\text{O}} \exp\left(\frac{-50,000}{RT}\right)}{1 + 3560 (P_{\text{H}_2} + P_{\text{CO}}) + 6950 P_{\text{H}_2\text{O}}} \quad (4)$$

where the rate R is given in terms of $\text{mole}/\text{cm}^2 \cdot \text{hr}$, the partial pressures are in atmospheres and temperatures in K. The calculation used published³ accelerated steam corrosion data² for ATJ graphite, the present leading candidate for the core support post material. Because of some uncertain features with respect to these published data, Eq. (4) must be taken as an approximation only.

Fig. 3.4. Core Post Burnoffs at End of 40-year life — For Nominal Core Post Temperature and Nominal Purification Flow.

The four curves shown in Fig. 3.4 pertain to four different estimates of the impurity atmosphere at the stated ingress rate. The curve labeled "Dragon" refers to burnoffs calculated from HTGR impurity levels predicted from Dragon steam ingress data as basis for predicting core reactivity to H_2O . The impurity levels used to obtain this curve are given in Fig. 3.1. The remaining three curves were obtained from impurity levels predicted with TIMOX for three assumed corrosion rate expressions for the core graphite.

Figure 3.4 shows that at low steam ingress rates predicted burnoffs range through about a factor of 5, depending on the assumed core reactivity, which determines the environment that the core post experiences. The range begins to narrow for assumed ingress rates of about 4 mg/sec and reaches only about 50% for ingresses of 40 mg/sec and above. The reason for this is that the ATJ corrosion expression tends to become zero order (i.e., independent of H_2O concentration) for the higher concentrations resulting from the higher ingress rates. Therefore, core reactivity is a less sensitive parameter at the higher values of steam ingress.

A maximum allowable core post strength loss of 50% due to corrosive effects was estimated on the basis of maximum allowable stresses given⁴ in GASSAR-6, estimated post loads, and material properties. The core post safety factor is approximately 10 at start of life and must never diminish below a safety factor of 5.*

The degree of burnoff estimated to cause the maximum allowable strength loss of 50% is indicated by the horizontal band in Fig. 3.4. This range was determined by setting the fractional strength loss (FSL) to a value of 0.5 in Eq. (3) and using the core post diameter, D , of 15 cm (6 in.). Solving Eq. (3) for the burnoff for 50% strength loss yields

*Review comments, however, have called attention to the fact that these values pertain to normal operating conditions. Maximum allowable strength loss during upset conditions is presently set below 50%, at approximately 22%. Therefore, continued reactor operation would require more stringent requirements on allowable core post strength loss than assumed in this study.

$$BO_{50} \left(\frac{\text{g}}{\text{cm}^2} \right) = \frac{3.68}{P(T)}, \quad (5)$$

where $P(T)$ are temperature dependent factors given in Table 3.2. For an assumed nominal core post temperature of 786°C, $P(T)$ is estimated to range between 5.95 and 14.0, which yields the indicated uncertainty band for 50% strength loss.

Figure 3.4 shows that for continuous ingress rates below 24 mg/sec there is a zero probability of 50% strength loss at the end of the 40-year life, for this case assuming nominal core post temperature.

Continuous ingress rates above 24 mg begin to show some overlap in the range of predicted burnoffs with the range, which could result in 50% strength loss. Hence, ingress rates above 24 mg/sec yield some nonzero probability of 50% strength loss. A general method for predicting the probability that an estimated range of burnoffs would cause 50% strength loss may be developed if one knows or assumes the probability density function within each uncertainty band. We have prepared a method based on an assumed constant probability density function for each case, equivalent to assuming that the actual burnoff may be with equal likelihood anywhere in the stated range, and the actual burnoff causing 50% strength loss could with equal likelihood be any value within the stated range (620 to 260 mg/cm² in Fig. 3.4). The probability for 50% core post strength loss is given by

$$P(50) = \frac{(y_2 - x_1)^2}{2(x_2 - x_1)(y_2 - y_1)}, \quad (6)$$

where

- $P(50)$ = probability for 50% core post strength loss,
- y_2, y_1 = upper and lower limit to predicted burnoff range,
- x_2, x_1 = upper and lower limit to burnoff range for 50% strength loss.

Equation (6) applied to the case shown in Fig. 3.4 where the error band for predicted burnoffs intrudes partially into the lower portion of the error band for 50% strength loss.

Applying values from Fig. 3.4 to Eq. (6) indicates that the probability of 50% core post strength loss at 1.0 g/sec continuous ingress rate equivalent to a total oxygen level of 850 vol ppm is 8%. For an assumed ingress rate of 0.1 g/sec corresponding to a total oxygen level of 85 vol ppm, Eq. (6) predicts a 2% probability of 50% core post strength loss. Ingress rates below 25 mg/sec, equivalent to 21 vol ppm total oxygen in the coolant, yield no overlapping region of estimated core post burnoff and range for 50% strength loss, hence show zero probability for 50% strength loss.

These results apply to the core support posts at the nominal coolant exit temperature from the core. Since the corrosion rate doubles approximately for each 30°C temperature rise under these conditions, the temperature variability in the core post regions assumes great importance. At this time the maximum sustained temperature experienced by the core support posts is not known; however a reasonable judgment may be made that is in the neighborhood of 10% above the average. Our analysis shows that the probability of 50% strength loss due to corrosion effects on the core posts is significantly higher for these hotter posts.

It should be emphasized that the assessment here summarized pertains to a support post design outlined⁴ in GASSAR-6, available during the time of the study. Design changes contemplated at this times include a change in core post material and an increase in diameter from the value of 0.15 m (6 in.) stated in GASSAR-6. The latter change will likely greatly affect the conclusions, and hence the assessment should be reviewed when the newer design becomes fixed.

3.5 REFERENCES

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4. SEISMIC AND VIBRATION TECHNOLOGY
PRELIMINARY ASSESSMENT OF THE GENERAL ATOMIC COMPANY CORE
SEISMIC PROGRAM

H. A. Nelms and J. P. Callahan

4.1 SCOPE

The ultimate objective of Task Area 4 of the ORNL HTGR Safety Program is to identify the HTGR components that have a safety-related function that might be subjected to potentially damaging vibration or seismic loadings and to assess the design criteria and activities directed toward mitigating the detrimental consequences of these loadings. In this chapter the GA core seismic program will be reviewed.

Many complex and especially unique engineering problems can be resolved by solving a set of simple problems that either bound the significant variables or provide the basis for a simplified model that includes the significant parameters. General Atomic expects to resolve the HTGR core seismic design problem using such an approach. The ongoing seismic program is oriented toward developing an analytical model, or models, that can be used to ensure that both safety and functional design criteria are fulfilled. Portions of the GA analytical and experimental programs have been completed, but a number of significant experiments and analytical developments have yet to be completed and/or documented.

The various aspects of the GA analytical and experimental program will be outlined in this report. Immediately following the scoping statement, a general description of the seismic design problem and the design criteria is given. That discussion is followed by a description of the GA experimental program by task area and a description of each computer program being developed. Finally, a summary and recommendations are given.

4.2 PROBLEM DESCRIPTION

The components of an HTGR core are routinely subjected to pressure, thermal, and gravitational loadings. These loadings induce strains and displacements, which, for the most part, can be evaluated by conventional

techniques to determine whether or not material or functional limits have been exceeded. Material limits are normally stated in terms of allowable stress, or number of cycles at a given stress intensity; and functional limits may be stated in a number of ways depending on the particular case — such as minimum clearance between mating parts or their alignment.

In addition to the consequences of ordinary loadings such as pressure, weight, etc., the effects of transient loadings occurring as a result of seismic events and flow-induced vibration must be evaluated. The central problem is to formulate for the core an evaluation procedure that can predict — for the various conditions of the core in the course of its life — forces, force repetition, strains, and displacements that can be combined with similar effects produced by the steady-state loadings and judged on a rational basis.

Once these fundamental quantities are determined, the combination of effects from the various loadings to which the core components will be subjected can be evaluated. It is these quantities that one will expect to see as a final product of the core seismic program.

Along the way toward final quantification of the effects of a seismic event, a great deal of information will necessarily be produced, especially by the experimental program. Impact characteristics, modal characteristics, and damping parameters will be output from the various experiments. Hopefully, both quantitative and qualitative information will be forthcoming that will increase confidence in the models used and permit further simplification of the models.

4.3 DESIGN CRITERIA

There are three general groupings of components within the PCRV central core cavity. They are (1) the core assembly, which consists of the fuel columns, the control fuel columns, and the removable side reflector columns; (2) the reactor vessel internals (as defined in the Summit PSAR), which consist of the core support structure, permanent side reflectors and borated shield, and the core lateral restraint structure; and (3) the reactivity control

systems, which consist of the control rods, the control rod drive, and the reserve shutdown system. The sensitivity of each of these groupings to a seismic event is different, as is the relative sensitivity within a grouping. Thus, the seismic design criteria of each grouping must be considered separately.

Seismic criteria for the components mentioned above are stated in both quantitative and qualitative terms. Generally, and especially for structural components such as core support posts and lateral support components, the seismic design criteria are stated initially in terms of a set of load combinations that include various combinations of normal and emergency loadings, including seismic loadings. Even qualitative criteria such as "no core element disarray" or "maintenance of sufficient coolant flow," which have been specified, will most likely be evaluated on the basis of load resistance.

At this time, a complete, detailed statement of the seismic design criteria for each component located inside the PCRV central core cavity is not in hand. The existing detailed core design criteria are expected to continue to evolve as additional data become available from the experimental program, because it is from the experimental program that characterizations of loading intensities and frequencies and kinematic relations will evolve. The analytical studies may lead to minor alterations in the core design criteria, but the likelihood of major changes in basic design criteria evolving from analytical efforts, as opposed to experimental studies, appear small at this stage in the GA program.

Some general observations will be made without benefit of a detailed statement of the seismic design criteria. A general discussion of the three major component groupings defined above is given in subsequent paragraphs.

4.4 CORE ASSEMBLY

The major components of the core assembly are the fuel columns, control fuel columns, and the removable side reflector columns. Each of the columns is topped with a keyed reflector element fabricated from steel stock and designed to meet the requirements of Subsect. NG Sect. III of the *ASME Boiler and Pressure Vessel Code*. The fuel and control fuel columns consist of eight active fuel blocks with two reflector blocks at the bottom and top of the fuel region. The removable reflector blocks are solid graphite. There are blocks of different length in each type of column.

Aside from the purely nuclear functions of these columns, they serve two fundamental engineering functions. A portion of the coolant flow channel network is provided by the columns, and the control fuel columns provide the channels into which control rods and reserve shutdown material can be inserted.

A general statement of criteria for the fueled columns, as stated in the *Summit Power Station Preliminary Safety Analysis Report* (PSAR) is as follows.

1. One-half safe shutdown earthquake: No core element disarray or damage shall occur such that normal full power operation cannot be maintained or resumed.
(upset condition)
2. Safe shutdown earthquake: The core elements shall retain their structural configuration to allow sufficient control poison to be inserted into the core to ensure safe shutdown and allow sufficient coolant to be maintained through the coolant channels to remove the reactor core decay heat.
(faulted condition)

The two items of seismic criteria enumerated above are stated to be for "fueled columns" in the Summit PSAR but use the terminology "core element," which is defined elsewhere in the PSAR to include removable side reflector columns. The enumerated items appear reasonable as general criteria statements for the fueled columns. It does not seem necessary, although we see no objection, to apply the same seismic criteria to the removable side reflector columns.

The "disarray" mentioned in the first PSAR statement, in reference to the core components, could in principle be produced by (1) shearing of the dowel pins between adjacent blocks in a column, (2) excessive rotation between adjacent blocks in a column such that the dowel pins are disengaged, or (3) a through-crack in a block. This last mechanism would appear to be unlikely unless a very large crack were already present at the time of the seismic event. The number of occurrences of any one of these events, or a combination of the three, that could be sustained without significantly affecting either the maintenance or resumption of full power should be one facet of the detailed criteria. Chipping or cracking short of through-cracks might occur without disarray, but this sort of damage does not appear to pose a threat to the structural integrity of the core.

For the safe shutdown earthquake (SSE) condition, the principal concerns are that (1) the control fuel columns remain sufficiently aligned to permit the insertion of the control rods and the reserve shutdown spheres and (2) that the core elements retain their structural configuration to the extent that adequate cooling is possible. Here again, shearing or disengagement of the dowel pins is a concern. Rotation (in a vertical plane) leading to insertion of the reserve shutdown spheres into the coolant stream is a possibility although apparently not a highly probable event.

If gross disarray does not occur, the major question appears to be whether or not the seismically induced motion will be sufficient to significantly impede the insertion of control rods. Since control rod insertion times of 25 to 35 sec are permissible and since this length of time is as long or longer than the duration of the probable earthquake, the answer to this question appears to be that an appreciable impediment to control rod insertion is permissible so long as insertion is not prevented.

4.5 REACTOR VESSEL INTERNALS

The principal reactor vessel internals are the core support structure, which consists of the core support blocks and the core support post, the permanent side reflector, and the core lateral support structure. The Summit PSAR states that the seismic loadings on these components are to be determined by the GA experimental program, with the results being supplemented by analyses.

The basic mechanical requirement of the core support structure is to provide vertical support during all conditions of loading and, in conjunction with the lateral restraint structure, provide the horizontal support for the lower end of the core. The Summit PSAR defines the design loadings as follows:

1. dead weight + weight of core + normal pressure drop + normal thermal loads + maximum predicted PCRV movements (normal conditions);
2. as under 1. + one-half safe shutdown seismic loads (upset conditions);
3. dead weight + weight of core + design basis accident pressure drop; and
4. as under 3. + safe shutdown seismic loads (faulted condition).

The safety factors incorporated into the design of the core support blocks and posts for normal operating conditions are stated by GA to be approximately 5 and 11, respectively. It is expected that the loadings produced by the SSE will significantly increase the peak stresses in these components, but that there will still be a safety factor of approximately 3 for the support blocks and approximately 7 for the posts. The safety factors stated for the posts relate to Hertzian stresses at the ends of the posts. The length-to-diameter ratio of the post, approximately 13, is such that buckling is highly improbable. These safety margins, if attained, are substantial but are justifiable on the basis of the important functions of these parts.

The core support columns are very simple structural members with respect to both loading and geometry and are apparently designed very conservatively. The peak stresses are Hertzian at the extreme ends. Consequently, localized failure at the extreme ends of the columns is the most likely stress-related failure. Unless environmental factors cause deterioration of mechanical properties, a mechanical failure of a core support post as a result of normal or emergency loadings appears unlikely.

One can postulate that, as a result of seismically induced motion, a core support post might become disengaged from the post sockets. Should one or more of the support posts in a localized region become disengaged, the consequences could be serious. This is not to suggest that such an event is probable or that there is even a significant probability of its occurring. Disengagement of a support post from its socket appears to be highly unlikely. However, the possible serious consequence suggests that specific criteria addressing this point should be developed and experimental evaluation should be considered.

The core support blocks serve two basic structural purposes: (1) to transfer the core and permanent side reflector loads to the support posts and (2) to provide horizontal support for the lower end of the core. The support blocks also serve as collectors for coolant flow from the core. As noted previously, preliminary results indicate fairly comfortable safety margins for both normal and SSE conditions. Here again, as for the core support posts, the potentially serious consequences of a failure dictate the need for specific design criteria and strongly suggest experimental verification.

The basic criterion for the permanent side reflector and borated shield is that they remain structurally sound during a seismic event. Presumably, some damage in the form of cracking, chipping, and even slight misalignment is permissible. So long as damage of that type is slight, the ability of the side reflector to restrain core motion would appear to be preserved and cooling passages would

not be significantly altered. Although normal operating conditions produce modest loads on both the side reflector and borated shield, seismic loads could be significant. These loads will be a matter of study in the GA experimental program.

During a seismic event, the core lateral restraints serve their basic restraint function and diminish the loading transmitted between the core and the PCRV. The magnitudes of the seismic forces transmitted by the lateral restraints are to be evaluated during the planned GA experimental program. The lateral restraint components are to be designed to meet the requirements of Section III of the *ASME Boiler and Pressure Vessel Code*.

4.6 REACTIVITY CONTROL SYSTEMS

The reactivity control system consists of the control rods, the control rod drive assembly, and the reserve shutdown system. For these systems to function, not only must their own seismic design criteria be met, but that of the core, and particularly that of the control fuel elements, must be met. The direct effects of seismic forces on the reactivity control systems do not appear to constitute a serious problem. Thus, if the core seismic design is adequate, the probability of successful insertion of both the control rods and the reserve shutdown spheres is high.

The design criteria for the control rods and drives that relate to seismic events may be summarized as follows:

1. The control rods and drives shall be designed to provide the normal mode of reactivity control during upset conditions.
2. There shall be no loss in reactor trip capability during emergency and faulted conditions.
3. Structural integrity shall be maintained during upset, emergency, and faulted conditions.
4. The control rods shall be capable of traversing the full length of the control rod channels by gravity fall under upset, emergency, and faulted conditions.

The criteria for the control rods and control rod assemblies may be sorted into functional and mechanical categories. The functional criterion relates to the kinematics of individual components and their interaction. The mechanical criterion that refers to load carrying capability appears to be the less worrisome of the two types of criteria. Both types of criteria are to be tested in the planned experimental program.

The reserve shutdown system is an elegantly simple system insofar as seismic events are concerned. The redundant actuation system minimizes the chance of a loss of actuation capability due to a seismic event. Inadvertent actuation due to seismic loading does not pose a safety question. The principal concerns, insofar as a seismic event is concerned, will be adequate design of the guide tubes and sizing of the shutdown material balls so that the likelihood of their escaping from their assigned channel in the control fuel elements is small. The guide tubes are simple, albeit important, components, which should present no design problem. Currently the plan is for the reserve shutdown material to be in the form of 14-mm-diam (9/16 in.) spheres. During a seismic event, each column element will rotate relative to its neighbors. This rotation poses the possibility of the spheres escaping their assigned channel into the space between adjacent columns. Thus, for any given size of sphere, a limit on the maximum rotation permissible between any pair of column elements will be imposed if loss of reserve shutdown material from its assigned channel is to be guaranteed by purely geometrical considerations.

The capability of maintaining the shutdown material in its assigned position should be addressed in the design criteria for the core. To some degree, the question of loss of reserve shutdown material will be answered by the proposed experimental program. Whether or not the geometry of the system provides sufficient control on the disposition of the spheres can easily be observed. It does

not appear possible, as a part of the planned experimental program, to investigate the effects of coolant flow on the disposition of the reserve shutdown balls.

4.7 EXPERIMENTAL PROGRAM

The usual course of an experimental program is to (1) state the objectives, (2) design the model, (3) perform the experiment, and (4) reduce the data to a usable form. At this point the data may be used to support analytical results, or the data may be combined with the results of other experimental and/or analytical studies to produce a single parameter of merit. One may decouple mathematically various aspects of a problem, but, in a practical sense, the problem remains coupled. Thus, the overall plan for an experimental program must be formulated with the end use in mind.

The main objective of the GA experimental program appears to be support and verification of the analytical tools that will be used in the design of actual components. However, some facets of the experimental program, such as the control rod and reserve shutdown material insertion tests, are directed toward specific proofs of functional capabilities, and the Summit PSAR suggests that the specific determination of loadings for some core support components is the aim.

The nature of the information desired from the various experimental tasks will be a major point of consideration for this assessment activity. For some of the proposed tasks, the nature of the information desired is clear. For cases where experimental models can, for practical purposes, be characterized as exact representations of the prototype, direct correlations can be made. Where it is necessary to deviate significantly from exact modeling, careful attention will have to be given to interpreting the resulting test data.

Both experimental and analytical engineering evaluations are to some degree exercises in modeling. Frequently, sophisticated analytical characterizations can be formulated, and, almost as frequently, one finds that for want of time, specific data, or

computer capability the sophisticated analytical formulation must be compromised. Thus, one resorts to using simple models coupled with greater safety factors. Extremely complex problems, such as the seismic analysis of an HTGR core, may require several simplified models to bound the significant variables. It is not necessary that each model be exact if it can be shown that the characteristic of interest is loosely coupled to parameters not modeled exactly.

The four general groupings of experiments being conducted by GA are (1) tests on specific components, (2) tests to determine basic characteristics such as contact time during impact and coefficient of restitution, (3) tests with subassemblies such as the column tests, and (4) tests on complete assemblies. Each grouping and the specific tasks within each grouping can be visualized as elements of the matrix that characterizes the HTGR core seismic problem. The object is not necessarily to identify all elements of the matrix but to ensure that the significant elements are defined.

We have not received specific documentation relating to tests of specific components. It is our understanding that, as a part of the GA-CEA joint effort, testing of graphite core support posts as a function of length-to-diameter ratio has been essentially completed and that tests to evaluate the performance of the core spring boundary design have also been completed. As documentation relating to tests of specific components becomes available, it will be included in this assessment program.

The remainder of the general groupings, consisting of items (2), (3), and (4), make up the subject of a ten-task experimental program. The following discussion of the tasks is based on presentations by GA personnel and the Summit PSAR.

Task 1. Collision Dynamics

The core of an HTGR consists of several hundred tightly packed, hexagonal columns of graphite. These columns are composed of fuel, reflector, and control fuel elements, which vary in length from

0.38 to 0.79 m (15-31 in.). The elements are stacked end-to-end to form the columns. Seismically induced motion of the columns will probably cause them to impact with each other and with the permanent side reflectors. To evaluate the consequences of the impact, it is necessary to develop an understanding of the impact parameters such as contact time, forces, and coefficient of restitution. The collision dynamics tests are for that purpose.

In these tests one pendulum-mounted graphite element is displaced from its equilibrium position and released so that it impacts with a second similarly mounted element. Impact velocity is varied by changing the position of the initially displaced element. Full, one-half, and one-fifth scale models of unirradiated graphite have been used. The hole pattern of the prototype is used in the full-scale model. In the one-half and one-fifth scale models the hole pattern is varied to obtain the correctly scaled mass and coefficient of restitution but the across-the-flat stiffness of the element is not scaled.

Impact velocity is varied and contact time and coefficient of restitution are studied. The coefficient of restitution has been found to vary from about 0.15 for an impact velocity of 1.5 m/sec (5 fps) to 0.35 for an impact velocity of 9.1 m/sec (30 fps). Contact time as a function of impact velocity was found to be highly nonlinear over the range of 1.5 to 3 m/sec (5-10 fps) but fairly constant beyond 3 m/sec (10 fps) to about 9 m/sec (30 fps).

Tests such as these provide data essential to the formulation of more complex experiments. Flat-face impact was the initial subject of study, but tests using other impact orientations were scheduled to be completed in early 1975. Since impact at many orientations may occur during an actual seismic event, additional tests of this type may be useful. Also, since characteristics such as coefficient of restitution are functions of the materials being evaluated, one must define such characteristics in terms of temperature, radiation dose, etc. or show by other means that the data taken can be conservatively used.

Task 2. Multiblock Collision Test

The tests conducted in this task area had the same general objectives as Task 1 tests. Here again, the across-the-flat stiffness of the blocks was not scaled, and the pendulum mounting arrangement used in the Task 1 tests was employed. These tests were scheduled to be completed in early 1975.

Task 3. Basic Rocking Test

The purpose of these tests, as for the tests in Tasks 1 and 2, is to provide fundamental information needed to formulate more complex experiments and to provide data required in the development of computer codes. In these tests various numbers of one-fifth and full-scale blocks are stacked vertically. The column is displaced, released, and allowed to "rock" until equilibrium is attained. Dowel forces and vertical forces acting on the blocks are to be monitored. It is not clear at this time whether or not coefficient of restitution, contact time, or damping will be studied. These tests were scheduled to be completed in late 1974.

Task 4. Single Column Test

This is one of a group of tests that may be classified as subassembly tests. In this effort a single, one-fifth scale column is subjected to vertical excitation, simultaneous excitation along the vertical and one horizontal axis, and simultaneous excitation along two horizontal axes. The extent to which impact with adjacent columns will be accounted for in the model or whether or not the core support structure is modeled very precisely is not clear.

Late in the fall of 1974 these tests were targeted for completion in early 1975. The studies in this task area will be closely aligned with the development of the COCO and SECA computer program series.

The tests planned for the Task 4 activity are an extension of a series of scaled-column tests in which sinusoidal excitations were imposed on the ends of scaled single columns of 14 fuel and

reflector elements. A one-fourth scale graphite column, one using an acrylic material, and a one-eighth scale graphite column were tested. The axial forces due to pressure drop across the core were simulated. The input frequency, displacement, and acceleration were varied during the test. These tests were directed toward developing an understanding of "column effects" on a planar array.

Tasks 5 and 6. Two-Dimensional (Horizontal Core Slice) Tests

These tasks are, respectively, one-fifth and one-half scale models of a horizontal slice of the core. The one-fifth scale models are one block deep and have 73 elements in the two-dimensional array. These are additional examples of subassembly tests. Here again, these tests will provide another stepping stone to three-dimensional experiments and will provide input for the development of the CRUNCH and COSAM computer program series. These tests were scheduled for completion in mid-1974.

The model used in the one-half scale tests can be visualized as a section of the core lying between two horizontal planes. Actually, there would be several hundred columns cut by this set of horizontal planes, but, to simplify the experiment, only 91 elements are used in this model. Uniaxial excitation was employed.

The objectives of these tests, as stated in the Summit PSAR, are as follows: (1) "obtain the effect of change in core gap on core resonance, fuel block impact response, and boundary response; (2) correlate results from two-dimensional analytical models with test results; (3) obtain further data in aid of verifying the model scaling factors for fuel block impact time, stiffness, and force as well as boundary support force and verifying block motion characteristics."

Task 7. Two-Dimensional, Two-Axis Test

This task is an extension of Tasks 5 and 6. The one-half scale, 91-element model is to be subjected to excitation (1) along an axis perpendicular to a flat of an HTGR core element, (2) along

an axis aligned "across corners" of an HTGR element, and (3) then simultaneously along two axes. Test (3), with properly chosen axes and loading, should give the same result as test (2). Again, this is a subassembly test, another stepping stone to three-dimensional tests, and the data obtained will provide input to the development of computer program CRUNCH 2D. These tests are scheduled for completion in early 1975.

Task 8. One-Fifth Scale Full Array Dowel Force Test

This is the first in a series of tasks that may be described as full assembly tests. The test model included a simulation of the PCRV, the lateral restraint components, permanent reflector, and core columns. The support columns were not included as part of the physical model. A wide range of data including boundary forces, displacements, and vibration characteristics was taken. These tests were conducted at Wyle Laboratories.

In this test program the model was excited along a single horizontal axis during any one specific test. The directions of excitation input were "across flats" and "across corners". The response of the model in a direction perpendicular to the axis of excitation was reported to be weak. Real time histories of earthquakes, synthesized time histories, and sinusoidal inputs were used as excitation inputs.

Task 9. One-Fifth Multiaxial Test

This is an extension of Task 8. For this task the model will be modified and multiaxial excitation will be employed. Changes in the model that are being considered include modifications of the spring packs and the core barrel and physical modeling of the support structure. Completion of this task is not expected until late 1975.

Task 10. One-Fifth Full Array, Control Rod Test

This is a further extension of Tasks 8 and 9. The objectives are to (1) determine the effects of the control rod and guide tube assemblies on the dynamic response of the HTGR core, (2) demonstrate control rod insertion during and after a seismic event, and (3) demonstrate reserve shutdown system operation during a seismic event. The model used in Tasks 8 and 9, with modifications and additions, will be utilized.

4.8 ANALYTICAL PROGRAM

The 2000-MW(t) HTGR core consists of 343 columns surrounded by an additional 72 columns of replaceable reflector graphite. Each column consists of 12 graphite blocks, so there will be approximately 5000 graphite blocks within the bounds of the core supports and the permanent reflector. A first reaction might be to select for analysis a model that represents each block as a lumped mass. If each of these masses were able to move vertically, move in each of two perpendicular horizontal directions, and rotate about all three axes, it would have 6 degrees of freedom. This would mean that the model would require approximately 25,000 degrees of freedom for the core and removable side reflector blocks alone. Additional degrees of freedom would be required for the permanent side reflector, plenum elements, and core supports. A model of this size would require a prodigious amount of computer storage and computation time and appears to be prohibitive from the standpoint of both time and cost.

Given this limitation, it seems that less extensive, approximate models that can reduce the number of degrees of freedom to a more manageable size must be used. Two approaches are possible. First, if certain aspects of the dynamic behavior of the core or parts of the core can be isolated, these effects can be investigated independently to determine their consequences and importance to the general core behavior. As an example, the response of one isolated column to a seismic excitation might yield data that would explain the dynamic behavior of such a column and suggest ways

of modeling the columns of the core structure with fewer degrees of freedom. Secondly, assumptions concerning the motion that might exist for certain elements of the core would allow groups of elements to be combined into fewer elements of larger size, thereby reducing the number of degrees of freedom in the dynamic mathematical model.

Seven analyses with associated computer codes are being developed by GA. These analyses fall into two groups. The first group attempts to model the reactor core as a number of adjacent rigid masses, each representing a group of columns, connected by a linear spring and viscous damping element to the PCRV. In this model, provision is made for collision between adjacent blocks characterized by either an effective coefficient of restitution or nonlinear springs and spatial gaps.

The second group of analyses is directed at predicting the dynamic behavior of a single column of blocks or a group of closely positioned columns of blocks. The blocks in any particular column are connected either by vertical-displacement-limited compression impact springs and rigid shear connectors, or by massless hinges, which allow blocks to rock one on the other. In the latter case the vertical impact is modeled again by an effective coefficient of restitution.

Acronyms have been given to the computer codes associated with the analysis of each of the seven mathematical models. A brief description of each of these follows

Group I Codes

PIC. In this code, which is not currently being used, each column of blocks is assumed to deflect as a beam supported at its ends and bending in its first normal mode of vibration in each of two transverse directions. If such an assumption is made, the deflected configuration of the column can be specified by the midspan deflection in each of the two horizontal directions. This is equivalent to a vibrating mass with two horizontal degrees of

freedom supported from the boundary by springs that simulate the stiffness of the column at midspan. Viscous damping elements connecting each mass with the boundary represent the energy dissipating mechanisms present in the vibrating column.

The possible collision of each mass with adjacent masses representing other columns must be taken into account. In this model this is done by letting each mass have a horizontal circular cross section (i.e., puck-shaped masses). The collision process is assumed to take place over very short periods of time, and consequently the momentum of the pucks involved in any particular collision is conserved. The energy lost during the collision is represented by an effective coefficient of restitution for the pucks. The excitation is applied by imposing on the boundary (PCRV) the appropriate acceleration versus time. The equations of motion are integrated numerically while intermittently considering the puck collisions as they occur. The results of such an analysis will be puck displacements, spring forces (stresses), relative velocities of adjacent pucks at impact, and the frequency of occurrence of the impacts.

COSAM. Each column is allowed to deflect in only one horizontal direction, and a number of columns are combined to form each block mass in the one-dimensional array. Again each mass in the array is connected to the boundary (PCRV) by a nonlinear spring and a viscous damping element simulating the column lateral stiffness and damping. The collision resilience is modeled by interblock springs that act in compression only and are loaded only after the relative position between the adjacent blocks decreases by an amount greater than a predetermined gap, which approximates the space between the HTGR core columns. No provision is made for energy loss due to impacting. The seismic excitation is again introduced by an appropriate one-dimensional acceleration-time history at the boundary. The result of this analysis gives element

displacements and velocities, interelement collision forces, and boundary forces that represent the forces between the reflector blocks and the PCRIV.

CRUNCH. The mathematical model for this analysis is, similar to COSAM, a one-dimensional array of mass elements each connected to the boundary by a nonlinear spring and viscous damping element. The mass collisions are assumed to take place instantaneously, and thereby momentum of the elements involved in the impact is conserved. In addition, an effective coefficient of restitution is used to account for the energy lost during the impact. The one-dimensional accelerogram excitation is applied at the boundary, and the equations of motion are integrated, giving predictions of the same quantities as COSAM.

CRUNCH 2D. In this analysis the collision dynamics is tested the same as it is in CRUNCH. The masses are arranged in a two-dimensional horizontal array, each connected to the boundary in the same way as in CRUNCH. The excitation consists of the simultaneous application of an acceleration versus time in each of the two horizontal directions at the boundary. This analysis will be used to obtain more information on the distribution of collisions among columns and the effects of simultaneous boundary accelerations in the two horizontal directions.

Group II Codes

COCO. A single column of blocks is analyzed. The blocks can rotate and move vertically and in one horizontal direction. The boundary acceleration excitation is either vertical or in one horizontal direction or vertical and horizontal simultaneously. As the blocks move they are allowed to rotate relative to each other to account for the possibility of one block rocking on top of another. As the block rocks about one edge and then rocks back, a vertical impact takes place when the horizontal faces meet.

The vertical impact is modeled by vertical impact springs and viscous dampers. Each block is connected to the blocks just above and below by dowels that allow relative rotation of the blocks but no horizontal differential movement of corresponding points on the faces of adjacent blocks at an interface. In addition, collision with the boundary is also modeled by the inclusion of impact springs and dampers between each block and the boundary. The results of this analysis will be vertical, horizontal, and rotational displacements of each block together with dowel shear forces and vertical and horizontal collision forces.

SECA. The SECA model consists of a stacked column in which the vertical impact between blocks due to rocking is modeled by using conservation of momentum and an effective coefficient of restitution. In the horizontal direction the collision of the column with the boundary is also modeled by using an effective coefficient of restitution. No momentum consideration is made for this impact because of the indeterminant mass associated with the boundary during the collision with a block.

Multi-COCO. The model used in this analysis is an extension of that for COCO. The model consists of a series of closely spaced columns arranged side by side. These columns represent the core columns and support structure in addition to the core lateral restraint structure. The block-to-block vertical impact and the impact between columns and between the outermost columns and the lateral restraint are represented by springs and damping elements. The results of this analysis will give more information about the dynamic forces in the interblock dowels and about the block displacements and the horizontal and vertical impact forces between blocks. In addition the analysis will give the dynamic forces that will be experienced by the core support blocks and core support posts.

Also, the predicted displacements will give information with which the possible disarray of the blocks may be investigated. Both vertical and horizontal excitation is applied.

Multi-SECA. The model used is a series of stacked columns as used in Multi-COCO. In this analysis the vertical and horizontal impact process is taken into account by the use of momentum conservation and an effective coefficient of restitution. The results of this analysis will be predictions of the same quantities as given by Multi-COCO. The cost of using this code is estimated to be much less than that of Multi-COCO.

4.9 SUMMARY AND RECOMMENDATIONS

This assessment is a sketch of the GA core seismic program. For the most part, its contents represent what could be drawn from several oral presentations, data given in the Summit PSAR, and several published papers. The general impression drawn is that GA has a vigorous and thoughtful program. The problem in hand is very complex and one for which there is no practical closed-form solution. Therefore, some uncertainty will be associated with whatever "practical" solution approach is pursued.

As noted previously, a thorough assessment of a problem solution can come only after all the pieces of the puzzle have been studied. The pieces of the puzzle in this case are documentation of experimental model development and analytical models, design criteria, and documentation of other analyses, the results of which will be combined with those of the seismic analysis. Since the GA program is now well along toward completion, it appears that much of the needed documentation should be available soon.

We are not presently in a position to recommend additions or modifications to the GA seismic program. As noted, the program appears, from the Summit PSAR and oral descriptions that we have received, to be fairly broad in scope. Prudent recommendations for additional tasks that might be added to the GA experimental and analytical programs cannot be formulated without a more comprehensive and detailed description of the existing program than can be gleaned from currently available documents. Similarly, timely recommendations for modifications to ongoing efforts are

difficult under the best of circumstances. When the dissemination of documentation lags significantly, the best that one can hope for is to influence the interpretation of experimentally derived data and of analytical determinations.

To advance beyond the impression stage, the assessment activity will need additional information in the form of documentation. It would be helpful to have a document that describes, in some detail, how all the pieces of the GA seismic program interact. A lag in documentation in an evolving program and, perhaps, a reluctance to disseminate preliminary documentation are understandable. However, if the assessment activity is to play a contributory role in the establishment of goals, an increased access to documentation is needed.

5. CONFINEMENT COMPONENTS — PCRVR TECHNOLOGY ASSESSMENT STUDY

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We are assessing the state of the art of prestressed concrete reactor vessel (PCRVR) development. The study is divided into the following areas:

1. concrete properties and construction practices,
2. analysis methods,
3. model testing,
4. new materials development,
5. instrumentation,
6. prestressing,
7. liners,
8. penetrations,
9. thermal barriers and cooling systems,
10. foundation and support systems.

Published material is being collected and reviewed, and we are preparing assessments of the various special areas. The following are summaries of the reviews that have been completed thus far.

5.1 STATE-OF-ART INFORMATION REVIEW

5.1.1 Materials Characterization

Available information concerning concrete behavior was reviewed to assess the constitutive relations incorporated in large-scale computer codes. Particular attention was given to time-dependent or creep behavior. Presently creep behavior is represented analytically by using an effective modulus of elasticity or using the principle of superposition of time histories together with a simplified account of aging and temperature variations. A rate-type creep law, which is

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applicable to mass concrete as well as concrete with moisture exchange, is available for implementation into finite element codes. Advantages of the rate-type as opposed to an integral-type creep law include more accurate modeling of behavior for variable temperature and humidity and reduced computer storage.

5.1.2 General Design Philosophy

The PCRV should respond elastically to short-term loads, whereas longer term stresses and strains are affected by creep and shrinkage of the concrete, relaxation of tendons, and possibly fatigue. Inelastic response should occur only under severe overpressure. Although permanent localized damage may result, the structure remains stable.

The analysis for service load conditions should include the temperature- and time-dependent characteristics of concrete. Net compression in the concrete should be maintained under service load conditions. Limited cracking may be acceptable provided that passive reinforcement is used and the integrity of the liner is not impaired.

Limit states must also be considered in design. Five identified limit states are:

1. limit of instantaneous linear elastic response,
2. limit of instantaneous reversible overall response,
3. limit of permissible deformation,
4. limit of liner defect stability,
5. limit (ultimate) strength.

5.1.3 Model Testing

The *ASME Code for Concrete Reactor Vessels and Containments* specifies that models must be used in the design process whenever accurate analytical procedures for the ultimate strength have not been established or whenever models of a prototype of similar characteristics have not been tested. The type of behavior being investigated determines the model size as shown below.

<u>Model Purpose</u>	<u>Minimum Dimensional Scale and/or Size</u>
Elastic response	1/14
Ultimate failure mode	1/14
Long-term loading	1/7
Temperature response (long term)	1/4
Temperature response (short term)	0.6 m (2 ft) thickness

Table 5.1 shows the models tested during the past 20 years, the type of tests, and research agency that conducted the test. Most of the tests listed were performed on single-cavity models with individual tendons oriented longitudinally and circumferentially. Six models of multicavity PCRVs have been tested; only one of them satisfied the size requirements of the *ASME Code for Concrete Reactor Vessels and Containments*.

There are four general categories of PCRv models. Small-scale models made of epoxy resin are used for determining elastic response, and small-scale models made of microconcrete are used to determine failure mode. Large-scale models made with essentially the concrete of the prototype vessel are used to study time-dependent response and behavior under thermal gradients. Models are also made of limited portions of the vessel to study in more detail localized conditions, such as head failure or stress concentrations in the vicinity of a penetration or cluster of penetrations. The practice in Europe has been to use 1/7 to 1/10 scale models for all PCRv structural response testing. It should be pointed out that thus far none of the multicavity PCRv models have been subjected to thermal testing. This appears to be a serious deficiency even though single-cavity model test data is relevant to a considerable extent.

5.1.4 New Materials Development

Alternatives to conventional reinforced concrete were reviewed to determine their applicability to the fabrication of reactor containment structures. Fiber reinforced concretes, hydrothermal and refractory

Table 5.1. PCRV Scale Models

Organization	Test item	Scale	Project	Number of models	Test for ^a
1. French AEC	Head, PCRV	Not known	G-2, G-3	2	A, B, C
	Cylindrical PCRV	1/10	G-2, G-3	3	A, B, C
	Cylindrical Vessels	0.76 m ID ^b 2.29 m high ^b	Safety Studies	25	C, D
	Cylindrical Vessel	Unavailable	G-2, G-3	2	A, B, C
2. Societe d'Etudes et d'Equipments d'Entreprises (SEEE), France	Cylindrical PCRV	1/6	EDF-3	3	A, B, C, D
	Cylindrical PCRV	1/10	EDF-3	1	T
	Cylindrical PCRV	1/5	EDF-4	2	A, B, C, T
	"Hot Liner" Vessel	Not known	General	1	A, B, C, T
3. Electricite de France (EDF) France	Cylindrical PCRV	1/5	Bugey I	2	A, B, C, T
	2 Layer Cylinder	1/3	General	1	
4. Central Electric Research Laboratory, England	Cylindrical PCRV	1/8	Oldbury	1	A, B, C, T
	Cylindrical PCRV	1/8	Pre-Oldbury	1	B, C
5. Sir Robert McAlpine & Sons England	Cylindrical PCRV	1/7	Oldbury	1	A, B, C, T, D
	Cylindrical PCRV	1/10	Hinkley Pt B & Hunterston B	1	A, B, C
	Multicavity PCRV	1/14	HTR	1	A, B, C
6. Taylor Woodrow Constr. Ltd., (TWC), England	Spherical PCRV	1/12, 1/40	Wylfa	2	A, B, C
	Cylindrical PCRV	Not known	Wylfa	3	A, B, C
	Cylindrical PCRV	1/10	Hunterston B	1	A, B
	Heads, PCRV	1/24	Several	12	A, B, C
	Multicavity PCRV	1/10	Hartlepool	1	A, B, C
	Head, PCRV	1/13	Pt. St. Vrain	2	A, B, C, D
	Multicavity, PCRV	1/30	GT-HTGR	2	A, B, C
7. Kier Ltd., England	Spherical PCRV	1/12	Wylfa	1	A, B, C, T
8. Atomic Power Constr., England	Cylindrical PCRV	1/10	Dungeness B	1	A, B, C
	Cylindrical PCRV	1/26	Dungeness B	1	B, C
	Heads, PCRV	1/72	Dungeness B	1	B, C
	Heads, PCRV	1/24	Dungeness B	3	B, C
	Heads, PCRV	1/26	Dungeness B	2	B, C
9. Building Research Station, England	Cylindrical PCRV	1/10	Hinkley Pt B	1	T
	Cylindrical PCRV	1/20	Hinkley Pt B	4	T
10. Foulness, England	Cylindrical PCRV	1/20	Study by UKAEA Safety Group	10 models to date (30 total 40% pneumatic)	C, D
11. General Atomic	Cylindrical PCRV	1/4	General	1	A, B, C
	Cylindrical PCRV	1/4	Pt. St. Vrain	1	A, B, C, D, T
	Multicavity PCRV	1/20	HTGR	1	A, B, C
12. Oak Ridge National Laboratory	Cylindrical PCRV	1/5	General	4	A, B, C
	Wall, PCRV	1/6	General	1	A, T
13. University of Illinois	Cylindrical Vessels		General	19	C, D
14. University of Sydney, Australia	Head, PCRV	1/20	General	#1	C, D
15. Siemens, Germany	Cylindrical PCRV (Prefabricated Blocks)	1/3		1	A, B, C
16. Krupp, Germany	Cylindrical PCRV	1/20	Gas-Cooled Reactor	1	A, B, C
	Head, PCRV	1/20	Reactor	1	A, B, C
17. ENEL/IBMES, Italy	Cylindrical PCRV	1/20	HTGR	2	A, B
	Head, PCRV	1/20	HTGR		C
18. Ohbayashi-Oumi, Japan	Cylindrical PCRV	1/20	HTGR	1	A, B, C
	Multicavity PCRV			1	A, B, C
19. Cement and Concrete Inst. Trondheim, Norway	Cylindrical PCRV	1/3.6	Scandinavian PCRV (LWR)	4	A, B, C
20. A. B. Atomenergi, Studsvik, Sweden	Cylindrical PCRV	1/2.5	Scandinavian PCRV (LWR)	1	A, B, T
21. Electric Power Development Co., Ltd. & Shimizu Construction Co., Ltd.	Cylindrical PCRV	1/10	Hinkley Pt. B	1	A, B, C
22. Nuclear Power Development Lab. & Kashii Kenetsu, K.K.	Cylindrical PCRV	1/20		3	A, B, T
23. PCRV Research & Development Group, Kajima Corporation	Cylindrical PCRV		ORNL Model	3	A, B, C, T
	Multicavity PCRV	1/20	GA 1100 MW(e)	2	A, B, C
24. Takenaka Technical Research Laboratory	Head, PCRV	1/20	General	14	A, B, C

^aA. Elastic response, B. Design overpressure, C. Failure, D. Abnormal conditions, T. Long-term creep and temperature.

^b2.5 ft ID by 7.5 ft inside height.

cements, and concrete with pozzolans appear to be potentially cost and performance effective. Fiber-reinforced concrete may have application in reducing the present congestion of conventional reinforcing in regions around penetrations and in minimizing the possibility of developing an undesirable shear failure in the head region.

5.1.5 Instrumentation

The various types of commercial instrumentation used to monitor the behavior of concrete structures were reviewed to determine their applicability to PCRVs. The conclusions of this study were: (1) techniques for measuring temperature, measuring prestressing forces, and detecting cracks are well established; (2) the effects of temperature and radiation and the long-term stability of concrete embedment gages used for strain evaluation need investigation; (3) the concrete embedment gages available for stress determinations are not completely satisfactory because of their large size, inability to measure tensile stresses, and relatively high cost; (4) the techniques used for concrete moisture measurements often measured a relatively unimportant type of moisture and were accurate only to within approximately $\pm 20\%$; and (5) acoustic emission is a promising technique for monitoring structural integrity; however it requires considerable study to determine whether the various types of events occurring in a PCRv can be separated and identified.

In general test data appear to be insufficient to evaluate performance of concrete embedment instrumentation over the projected 20 to 30 year lifetime of a PCRv. And a gage for monitoring the stresses in the concrete must be developed.

5.1.6 Liners

Liner design criteria have been reviewed on the basis of published information on liners and liner anchorage systems for both PCRVs and reactor containment structures. Although some aspects of liner behavior require further study, it was generally concluded that the present design criteria adequately reflect the published test data

and general state of knowledge. Because of the high cost of present cold liner systems there is need for developing a less costly hot liner system.

5.2 REVIEW OF WORK IN PROGRESS

Material has also been obtained directly through discussions with experts in the field and attendance at technical meetings. The following are summaries of the information obtained.

5.2.1 PCRVR Development in Germany, France, and England

We visited a key group of establishments in France and England that have been closely involved in PCRVR development from the very beginning of the technology, and others in these two countries and in Germany that appear to be at the forefront of new developments in confinement components technology. The information obtained thereby should form the basis for a reasonable assessment of the present state of the art of confinement components in Europe.

Operating PCRVRs have experienced problems, the most serious of which appear to have been caused by oversight in the basic design and/or operation procedures; for example, the tendon failures at Marcoule, which resulted from indefinite deferral of implementing the planned corrosion protection system; corrosion of exposed tendons at Wylfa; problems in constructing the first multicavity PCRVR at Hartlepool caused in part by a very late change in the design of the steam generator cavity closures; and the problems at Dungeness resulting from designing and constructing a vessel of inadequate size to contain the enclosed reactor system. The French appeared to be quite satisfied with their PCRVRs including the Marcoule G-2 and G-3 vessels, which were the first two PCRVRs constructed. The English have had considerably more problems, but they appear mostly optimistic that the multicavity PCRVRs will be successful. The most serious criticism of PCRVRs appeared to be that they are expensive; however, it was generally agreed that as larger reactors are developed, they should become competitive both for gas-cooled reactors and as replacements for conventional steel vessels, both in nuclear and nonnuclear applications.

With respect to PCRV innovations, the greatest interest appears to be in developing hot liner systems for use with either gas-cooled or light-water reactors; however, innovations are needed to produce a more practical and economical liner system.

Germany has a most comprehensive program for PCRV research and development. The research program consists of experimental investigations of concrete multiaxial strength, creep, moisture migration, and irradiation effects and an extensive analysis methods development effort. Also included are developmental studies of steel-fiber-reinforced concrete, hot liners, and instrumentation. Germany is studying many new vessel concepts for process heat, coal gasification, steam and gas storage, LWRs, and gas-turbine HTGRs. In addition, attention is being given to special vessels designed to provide protection from catastrophic failure such as might occur with a conventional steel vessel. One of the most interesting and promising innovations is the prestressed cast-iron vessel made of precast segmented components and being developed by Siempelkamp in Germany.

5.2.2 PCRV Concrete Materials Evaluation Studies

Concrete materials evaluation studies have been conducted at the University of California for the proposed Delmarva and Fulton HTGR power stations. These studies are intended to provide complete material characterizations. However, once a range of values has been established for the thermal characteristics, concrete testing for future plants would be reduced to evaluations of mechanical properties and creep behavior, providing the compositions of prospective concrete aggregates do not differ appreciably from those of the present studies. The more routine concrete testing for future plants could be done by existing commercial construction materials testing laboratories.

Although the University of California studies do not include triaxial creep tests, reasonable justification for excluding these more complicated tests is based on findings of the ORNL concrete creep testing program. Short-term triaxial strength tests were also not

included in the present studies. Unfortunately the relationship between uniaxial and multiaxial strength is not as clearly understood as in the case of creep behavior; however, it is recognized that the use of uniaxial strength data provides a degree of conservatism in the design of a PCRV. There is need for reliable multiaxial strength data to quantify this conservatism.

The studies being conducted by the University of California for GA consist of the following activities:

1. Development of three concrete mix designs
2. Evaluation of material properties
 - (a) Two aggregates
 - (b) Two cements
 - (c) Admixtures
 - (d) Three concrete mixtures
 - (1) Compressive strengths (7-, 28-, 60-day moist cure)
 - (2) Moduli of elasticity, Poisson's ratios (28-, 60-day moist cure)
 - (3) Drying shrinkage (to 60 days)
 - (4) Thermal expansion [aggregate evaluation, three cycles 23-71-23°C (73-160-73°F)]
 - (5) Heat of hydration (calorimeter)
 - (e) Properties of fresh concrete
3. Study of concrete properties
 - (a) Compressive strength (7-, 28-, 60-, 90-, 120- 180-, 270-, 365-, 730-day moist cured and sealed)
 - (b) Modulus of elasticity (28-, 60-, 90-, 270-, 730-day moist cured and sealed)
 - (c) Tensile strength (28-, 60- 730-day moist cured and sealed)
 - (d) Effect of water-to-cement ratio on strength [50-, 130-, 200-mm (2-, 5-, 8-in.) slump; 28- and 60-day cure]
 - (e) Effect of thermal cycling on compressive strength, modulus of elasticity, Poisson's ratio, and thermal expansion
 - (f) Thermal expansion
 - (g) Creep behavior [23, 43, 71°C (73, 110, 160°F); 60-, 365-, 1000-day loading, and concretes of different ages]

- (h) Autogeneous length change
- (i) Adiabatic temperature rise
- (j) Specific heat (at 28 and 90 days)
- (k) Thermal diffusivity (at 28 and 90 days)

5.2.3 Discussions at General Atomic Company

Meetings were attended at General Atomic Company twice during the reporting period. The principal discussion topics were PCRVR liner design and analysis methods.

The basic philosophy of liner design was described as using the prestressing loading to put the liner in compression. The liner is designed to withstand strains greater than yield without buckling. The liner corners are rounded at most places but squared at penetrations. The haunch and penetration regions of the liner are analyzed by three-dimensional finite element analysis methods. Subsequent vessel pressurization reduces the compression in the liner. A thermal barrier consisting of insulation and cooling tubes is used to limit the temperature of the liner to 82°C (180°F) to prevent excessive heating of the concrete.

Cooling tubes are used on 0.1 to 0.2-m (4-8 in.) centers with Nelson stud anchors embedded in 0.23 and 0.38 m (9 and 15 in.) lengths. The design calls for one stud for each 0.035 m² (55 in.²) of liner in order to resist a pressure of 0.86 MPa (125 psi) caused by leakage of cooling water between the concrete and liner. Two independent cooling water loops are used, with each being capable of handling the cooling load alone.

The anticipated worst loading for the liner is after both prestressing of the vessel and heat-up, but before pressurization. The liner temperature is permitted to rise to 121°C (250°F) at localized hot spots resulting from cooling tube failure, which actually means that the temperatures would be 132°C (270°F) between tubes, 93°C (200°F) at the tube, and a local hot spot of 191°C (375°F). The last temperature is termed the "extreme environmental temperature limit."

A minimum operating temperature of 38°C (100°F) is also imposed to keep the liner at 33°C (60°F) above the nil-ductility temperature.

Tests are being conducted by CEA at Saclay, France, to further characterize the buckling strength of the anchored liner under biaxial loadings. Some of the tests include simulation of the effects of a 0.20-m (8-in.) void between the concrete and liner, and pressurization of both sides of the liner to simulate loadings produced by pressurized helium on the inside and pressurized water on the outside.

The following GA computer codes are used in the design of PCRVs:

SAFE/2D is a two-dimensional elastic code for analyzing axisymmetric or plane sections. This code is similar to the ORNL version but has been improved by adding (1) a Choleski-method solution routine, (2) a shear deflection bar and a gap friction element for modeling liner-vessel interactions. This code is the primary tool for conducting two-dimensional analyses of PCRVs.

SAFE/CRACK is a linear viscoelastic analysis with cracking. This is a two-dimensional stress code for analyzing axisymmetric and plane sections. Although similar to the current ORNL version, it has been improved by adding (1) an improved iteration procedure for plasticity and (2) bilinear plasticity representation, which is restricted to isotropic hardening.

SAFE/3D is an elastic three-dimensional code based upon the constant strain tetrahedra. This code has been upgraded to the SAFE/SOLIDS code and is no longer supported at GA.

SAFE/SOLIDS is an elastic three-dimensional code based upon tetrahedral elements with either three degrees of freedom (displacement) or 12 degrees of freedom (displacement and displacement gradients) at each node. Triangular plate and rebar elements are included, which also have either 3 or 12 degrees of freedom at each node. Curved boundaries are available for the solid elements by specifying a center of curvature and radius of curvature for the element face.

Although these codes have the capability of computing results within the accuracy limitations inherent in the implemented constitutive relations, they have the following undesirable features.

1. Their limited element libraries do not offer the flexibility in element selection necessary to achieve an efficient mesh. This is reflected in increased computing cost.

2. The use of explicit rather than numerical integration in element stiffness formulation makes the introduction of new elements and modification of existing element formulations difficult. This hinders modernization of the codes and tends to freeze technological development.

3. The general logic structure of these codes is poor by modern standards; consequently it tends to hinder modernization efforts.

General Atomic Company has recognized these shortcomings and has undertaken the development of a modern code.

3D CREEP-CRACK is a modern three-dimensional code based upon isoparametric element formulations. The code actually consists of a package of codes. The ultimate load analysis capability is based on an incremental nonlinear approach with equilibrium checks, which permit the approximate element stiffness to be used. Periodic stiffness updates are accomplished by a direct modification of the factored stiffness so as to avoid reassembling and factoring of new equations. The cracking criteria are based primarily on *ad hoc* logic. It is assumed that the failure surface is a function of the first two principal invariants of the stress tensor.

In designing a PCRV, an elastic analysis is used in sizing for operating conditions. The code SAFE/2D is used for the initial analysis by employing a modified stiffness (smearing technique) in the axisymmetric sections containing penetrations. Critical sections are checked and modifications are suggested to eliminate high stress concentrations. The final elastic analysis is made with SAFE/SOLIDS.

Limit analysis of the barrel is performed by use of SAFE/CRACK, and empirical analysis techniques such as the University of Illinois' cryptodome method are used for the head region. Creep effects are bracketed by use of the effective modulus approach. Minimizing the amount of time-dependent analysis performed is probably preferred because of the high cost of performing such analysis using the memory

integral procedure contained in SAFE/CRACK. As the recently developed modern 3D CREEP-CRACK is brought into use the present time-dependent analysis practices of GA will change.

5.2.4 Meeting of the ACRS High-Temperature Gas-Cooled Reactor Subcommittee, Denver, Colorado, January 30-31, 1975

Topics discussed at this meeting having specific relevance to the PCRVR technology assessment study are (1) vessel overpressurization, (2) PCRVR model studies, (3) prestressing systems, and (4) closure design and testing.

1. Vessel Overpressurization. The block valves for isolating the vessel pressure relief valves and possible consequences of water ingress were discussed as potential causes of vessel overpressurization. Since the block valves are open during normal operating conditions and are closed only to permit testing of one relief valve at a time, it would appear that both valves could not be closed simultaneously as long as the interlock system functions satisfactorily. There still appear to be questions concerning whether the interlock system can be made fail-safe.

The six events considered by GA as causing possible water ingress into the PCRVR are (1) offset rupture of a superheater tube, (2) small tube leak, (3) failure of tubesheet extension cylinder, (4) offset rupture of a feedwater tube, (5) multiple-tube rupture of less than four tubes, and (6) rupture of four or more feedwater tubes. The quantity of steam required to reach 1 MCP (maximum cavity pressure) was given by GA as 140 kg/sec (310 lb/sec); to reach 2 MCP would require a flow of 280 kg/sec (620 lb/sec). Therefore, an enormous quantity of steam appears necessary to reach 2 MCP, the specified minimum PCRVR ultimate strength.

With respect to design of the steam generators, GA feels that its present development program is adequate, and that a steam line outside the PCRVR would most likely fail as a result of any catastrophic event that could produce wholesale failure of a steam generator. The ACRS subcommittee expressed concern that the regulatory staff appears to

be using only the appropriate ASME code design criteria to judge adequacy of the design, and indicated that the water ingress problem will be given further study.

2. PCRV Model Studies. Three multicavity models have been tested thus far in conjunction with the GA work. The largest was the 1/10-scale Hartlepool model, with the GA and Ohbayashi-Gumi models both being 1/20 scale. Although the latter two more closely represent the Summit and Fulton plant designs, all three are reasonably representative of the basic multicavity PCRV configuration, as shown in Table 5.2. The Japanese model study was summarized at the Fédération Internationale de la Précontrainte (FIP) Congress in New York on May 29, 1974; however, GA has obtained the detailed test data for all three models. The 1/20-scale GA model was fabricated with a slip plane between the barrel and heads. The models were made to conform as closely as possible to the PCRV geometry and materials representation. Although the models serve to demonstrate short-term pressure response of specific vessel configurations, GA indicated that the primary purpose is to provide reliable test data for verifying analysis methods, which are then used in the design analysis of prototypical PCRVs. Good agreement was reported between two-dimensional and three-dimensional elastic finite element analyses; however, the three-dimensional crack analysis code is not yet in good shape. The analytical work supported by Regulatory at the Naval Ordnance Laboratory and using NASTRAN was mentioned; however, thus far, only single-cavity PCRV configurations have been analyzed. The ACRS subcommittee expressed concern that no groups outside GA presently have the capability for analyzing multicavity PCRVs.

In discussing response of a PCRV to pressure loadings, GA indicated that the maximum cavity pressure or MCP value will exceed design pressure by the 2 to 3% tolerances of the relief valve train and back pressure of the gas line. General Atomic expects the PCRV to survive pressures well in excess of 2 MCP and indicated that vessel behavior could be predicted to 2 MCP providing the PCRV does not differ greatly from the model used to verify the analysis. However, GA felt that the actual point of failure could not be predicted on a representative pressure

Table 5.2. Comparison of Model Representations of PCRV Geometry

Parameter	Hartlepool ^a 1/10th Scale Model	GA 1/20th ^a Scale Model	Ohbayashi- ^a Gumi 1/20th Scale Model	Summit PCRV	Fulton PCRV
Dimensions, m (in.)					
Overall height	29.26 (1152)	24.99 (984)	27.00 (1063)	27.79 (1094)	27.79 (1094)
External diameter	25.91 (1020)	27.43 (1080)	30.00 (1181)	29.26 (1152)	30.63 (1206)
Core cavity height	18.29 (720)	13.61 (536)	13.79 (543)	14.43 (568)	14.43 (568)
Core diameter	13.11 (516)	10.80 (425)	12.27 (483)	7.67 (302)	11.28 (444)
Steam generator diameter	2.74 (108)	3.89 (153)	3.99 (157)	4.34 (171)	4.34 (171)
Wall thickness	6.40 (252)	8.15 (321)	8.79 (346)	9.65 (380)	9.68 (381)
Head thickness	5.49 (216)	5.69 (224)	5.59 (220)	5.64 (222)	5.64 (222)
Head depth/span ratio	0.42	0.53	0.45	0.57	0.50

^aFor comparison, the model dimensions were multiplied by the appropriate scale factor.

versus deflection curve. It was pointed out that failure could mean either leakage or more disruptive structural failure. With respect to the model tests, the liners eventually ruptured and the rubber bags punctured until the rate of leakage exceeded pump capacity. This type of failure is in agreement with the PCRV design criteria, which call for gradual observable behavior as the ultimate strength is approached. It was pointed out that vessel leakage cannot be predicted from models, although it would be desirable to be able to predict both vessel leakage and liner stresses. The existing design criteria call for a maximum allowable rupture area, such as would be caused by failure of a closure primary hold-down, to be limited to 0.0645 m^2 (100 in.^2).

Although the ultimate capacity of the cylindrical vessel wall cannot be accurately predicted, a reasonable lower bound was given as 2.5 MCP. Prediction of head failure is even more difficult; however, GA indicated that a reasonable lower bound could be determined. One possible area of concern is whether the lower bound value of the head can be less than the upper bound of the side walls, and thus result in an undesirable failure in the PCRV head.

3. Prestressing Systems. The stated difference between behavior of axial prestressing tendons passing through the PCRV support structure and those passing only through the vessel was indicated as negligible by GA. The circumferential prestressing is wound on precast concrete panels lined with steel, using a maximum of 18 layers of prestressing. The circumferential tendons are restrained by tapered anchor units consisting of a wedge and sleeve. The accuracy of the hydraulic load control system of the tendon lay-down machine was indicated by GA as -0% to +6%. Certified load cells are used to monitor the tendons during lay-down, and other load cells are placed beneath the strand to monitor in-place stress. The axial tendons will be monitored with the type of load cells developed for Fort St. Vrain.

Corrosion protection consists of coating the tendons with a commercial protective agent as the tendons are laid down, with each layer being inspected visually afterward. The wound segments are sealed by cover

plates having caulked joints. Untensioned corrosion test specimens are included under the cover plates and in the axial tendon ducts. The prestressing system is designed to permit retensioning of both axial and circumferential tendons. Additional prestressing can be wound on top of existing circumferential layers if this appears desirable. In general, the prestressing system described by GA appears to be acceptable; however, the inclusion of stressed corrosion test specimens for both the wound segments and linear tendons is most desirable.

4. PCRVR Closure Design and Testing. The steam generator cavity plug for the Summit plant PCRVR is 4.3 m (14 ft) in diameter, is 3.4 m (11 ft) thick, and has a 1.68-m-diam (5.5 ft) penetration in the plug. The concrete and liner are designed according to Section III, Division 2 of the *ASME Boiler and Pressure Vessel Code*, while the bolted flange primary hold-down and other steel portions not backed by concrete are designed according to Section III, Division 1. The loading path for the secondary hold-down system is through the bayonet blocks, to the shear anchors, and eventually to the prestressing tendons. The ultimate strength of the plug was stated to be approximately 2.5 MCP. Helium flow area in the event of primary hold-down system failure is limited to 0.0645 m² (100 in.²) by a ring-type flow restrictor. The concrete bearing stresses produced at the tendons was limited to the Section III, Division 2 allowable limits for bearing and break-outs; GA has also conducted edge loading tests of concrete to determine a satisfactory design value. The ACRS subcommittee questioned whether model tests of the plug restraint system would be required in addition to the planned plug tests. The plug and its restraint system appear to be one of the critical components of the PCRVR. Although GA had not done any rigorous analyses to determine the failure mechanism of the closure, it was estimated that initial failure would probably occur in the helium circulator penetration. In the event of a primary hold-down system failure, GA indicated that the resulting controlled leakage of helium should have no adverse consequences for vessel pressures up to 1.5 times the design value.

The planned 1/14-scale model tests of both primary and secondary hold-down systems are scheduled to be conducted by CEA in France starting in the spring of 1976. Although larger scale tests have been mentioned in other discussions with GA, they were not included in the time schedule for the Summit Power Plant closure development; however, proof tests of the prototype plugs were indicated. The ACRS subcommittee indicated that it will require more information on the design of plug shear anchors.

5.3 PCRV SAFETY ANALYSIS STUDIES

The initial goal of the planned analytical studies is to conduct elastic analyses of the Delmarva multicavity PCRV design to determine potential failure modes and to identify any portions of the vessel that are overstressed when subjected to anticipated design and off-design loadings. The three-dimensional finite element analysis computer program NØNSAP has been selected for conducting the initial elastic study as well as follow-on studies of nonlinear behavior.

5.3.1 Analysis Method Selection and Refinement

During the present reporting period we have studied NØNSAP in depth and have modified portions of the code to provide capability for more accurate analyses of the PCRV. The following describes the code and the changes that we have implemented.

Program NØNSAP is a general-purpose finite element program for conducting both linear and nonlinear static and dynamic analyses. It was designed to effectively solve a variety of practical nonlinear problems using current nonlinear analysis procedures and computer equipment and to develop improved nonlinear analysis techniques. The modular structure of NØNSAP makes it particularly useful for both purposes.

The NØNSAP element library includes the following three-dimensional elements:

1. truss (used for modeling skew boundary conditions and reinforcing bars),
2. three-dimensional solid, and
3. three-dimensional thick shell.

An in-core routine is used for solving the system of equations. This restricts the number of degrees of freedom in the system and eventually may cause difficulty in modeling the PCRv. The following describes the code and explains the changes that have been implemented.

The three-dimensional solid or thick-shell element in NØNSAP is a variable-node isoparametric element. In its simplest form it is an eight-node linear displacement field element. The isoparametric element formulation employs a normalized basis coordinate system to express the functional representation of the shape functions, which are the interpolating functions for the displacement field:

$$\vec{\sigma}(r,s,t) = \sum_{i=1}^n \{h_i(r,s,t)\}^T \{\vec{\sigma}_i\} ,$$

where n is the number of nodal points associated with the element, $\vec{\sigma}(r,s,t)$ is the displacement vector, $\vec{\sigma}_i$ is the value of the displacement field at the i th node point, $h_i(r,s,t)$ is the i th shape function for the element, and r,s,t are variables with range -1 to $+1$ (Fig. 5.1). These shape functions are also used to describe the element geometry in the global coordinate system in terms of the coordinates of the node points of the element:

$$\vec{P}(r,s,t) = \sum_{i=1}^n \{h_i(r,s,t)\}^T \{\vec{P}_i\} ,$$

where $\vec{P}$ is the position vector of a point in the global system and $\vec{P}_i$ is the position vector of the i th node of the element.

The use of the high-order isoparametric element provides a means of representing the geometrical outline of the PCRv and simultaneously modeling the strain gradients with relatively few elements; however, because of the high cost of formulating the element stiffness and the large number of degrees of freedom associated with these elements it is desirable to utilize them efficiently. Thus, it is necessary to have

Fig. 5.1. The Isoparametric Mapping.

available the special elements required to make transitions in the element mesh. These elements are of two types: hierarchical and degenerate. The hierarchical elements are ones for which the number of nodes and associated order of the shape functions may be different along different sides of the element (Fig. 5.2). By using these elements, additional degrees of freedom may be introduced where higher order strain gradients are expected. The degenerate elements are those for which two adjacent corner nodes are coalesced as shown in Fig. 5.2. These elements are required at the centerline apex of three-dimensional cylindrical models and at certain regions of mesh transition.

In formulating the stiffness matrix of a degenerate element special modifications to the shape functions are necessary to preserve interchangeability of the vertices and, therefore, prevent biasing of the stiffness matrix. Interchangeability of the vertices is characterized by specifying the same form for all corner node shape functions having all mid-size node shape functions, respectively. For example, the shape functions of the two-dimensional triangle degenerated from an eight-node isoparametric, shown in Fig. 5.3, clearly do not satisfy the condition of interchangeability of the vertices. To alleviate this problem the shape functions must be modified in a special way to form the required degenerate of the element. Appropriate changes have been made in NONSAP to permit the use of degenerate elements.

(a) HIERARCHICAL ELEMENTS

(b) DEGENERATE ELEMENTS

Fig. 5.2. Typical Hierarchical and "Degenerate" Elements.

Fig. 5.3. Shape Functions for Degenerate Triangle.

5.3.2 Analysis of Ohbayashi-Gumi Test Vessel

To provide a measure for verification of NØNSAP as our PCRV analytical tool, we analyzed a vessel for which experimental data exist. The Ohbayashi-Gumi multicavity test vessel was chosen for this purpose since a complete set of test data for this model appeared to be available. Presently, the experimental data we are using are from a paper presented at the Seventh Congress of The Fédération Internationale de la Précontrainte.¹

5.3.2.1 Description of Analytical Models

The test vessel was analyzed at Ohbayashi-Gumi with a three-dimensional finite element model representing a 30° segment extending the full height of the vessel and including the support structure. This model incorporated 166 elements and 1168 nodes.

We have analyzed the test vessel using two different analytical models. Both models represent a 30° segment of the vessel assuming symmetry at the midplane of the reactor cavity. It should be noted that the mid-height symmetry assumed for this model represents only an approximation of the actual vessel geometry. The bottom head of the vessel is one-third thicker than the top head and is further stiffened by the support structure. In addition, the penetrations in the top head of the vessel were not included in the analytical models; this also tends to increase the stiffness of top head. Both analytical models differed in geometric detail and ability to represent high strain gradients. Model 1 has greater geometric detail than model 2 (Fig. 5.4). The bearing pads for the vertical prestressing and the steel closure plates above the steam generator cavities were included in model 1 but omitted from model 2. However, model 2 can accommodate higher strain gradients than model 1 since 20-node quadratic displacement elements were used in this model, whereas, model 1 generally employed eight-node linear displacement elements. As much detail as possible was included in both models while still allowing the resulting system of stiffness equations to fit in the computer core, which is the restriction imposed by the solution routine of NØNSAP.

Fig. 5.4. Finite Element Analytical Models of Ohbayashi-Gumi PCRV Model. (a) ORNL model 1, (b) ORNL model 2.

The results of the analysis of model 1 are presented in Fig. 5.5 together with the results of both the Ohbayashi-Gumi analysis (indicated as OG on the figures) and the experimental data. The deflections and strains are plotted at their relative locations on the vessel cross section, which includes the top head and a portion of the adjacent wall. Figure 5.5(a) shows a vertical plane passing through a steam generator cavity while the remaining parts of Fig. 5.5 show a vertical plane that does not intersect a steam generator cavity. Differences between the ORNL analysis, the experimental values, and the OG analysis can be explained in part by the assumption of mid-height symmetry in the ORNL model.

Comparison of the two sets of analytical results in Fig. 5.5(a) shows that the ORNL analysis yields larger radial displacements of the outer wall at mid-height and greater vertical displacements in the outer region of the head than those given either by the OG analysis or the experimental data. The overly large radial displacements at mid-height can be attributed to the reduction in the stiffness of the bottom head that is implied by the assumption of mid-height symmetry. The deflection of the top head indicates

Fig. 5.5. Deflections and Stresses Calculated with ORNL Model 1 Compared with Ohbayashi-Gumi Analysis and Experimental Measurements. (a) shows a plane through a steam generator cavity; the other parts are for planes not intersecting a steam generator cavity. The pressure in (a), (d), and (e) is 4.9 MPa. The scales shown correspond to a deflection (strain) of 0.1 mm (100 μm).

that the ORNL model was excessively stiff as expected since penetrations were neglected. Comparison between the ORNL analysis, the OG analysis, and the experimental strain values indicates relatively good agreement with the exception of the axial strains due to vertical prestressing, Fig. 5.5(b), and the hoop strains along the bottom surface of the top head due to internal pressure, Fig. 5.5(e). In the former case the discrepancy is likely due to either the stated value of the elastic modulus being too small or the indicated values of the axial prestress loading being too large. For the latter discrepancy, the hoop strains along the bottom head are primarily due to the radial component of displacement, that is

$$\epsilon_{\theta} \approx u/r ,$$

where ϵ_{θ} is the hoop strain, u is the radial displacement, and r is the distance from the centerline of the vessel. Under internal pressure loading the radial displacement is outward and the resulting hoop strain must be tensile; hence, the OG analysis appears to be in error.

The two ORNL analyses are compared in Fig. 5.6. Fig. 5.6(a) shows that the outer wall of model 2 deflects more than model 1 and the top head bends more. This result is to be expected since the 20-node elements used in model 2 can accommodate the liner strain gradients due to bending, while the eight-node elements used in model 1 are essentially constant-strain elements.

Comparison of the strain results shown in Fig. 5.6(b) through (e) shows relatively good agreement with the exception of the hoop strains due to circumferential prestressing, Fig. 5.6(d). This discrepancy is probably due, almost completely, to the differences in flexibility between the two models. Larger radial displacements are produced in the more flexible model 2 under this loading, giving rise to larger values of the compressive hoop strain.

Fig. 5.6. Comparison of Deflections and Strains Calculated with the Two ORNL Finite Element Models. (a) shows a plane through a steam generator cavity; the other parts are for planes not intersecting a steam generator cavity. The pressure in (a), (d), and (e) is 4.9 MPa. The scales shown correspond to a deflection (strain) of 0.1 mm (100 μ m).

5.3.2.2 Conclusions

Both the ORNL analytical models are too coarse (i.e., they are incapable of accurately representing the behavior of the actual vessel). Model 1 represented the geometry well, but its eight-node elements could not accommodate the actual strain gradients. Although model 2 could accommodate higher strain gradients, it lacked geometric detail. Both models had added error caused by the assumption of mid-height symmetry. To achieve a more accurate analysis of the vessel will require a more refined analytical model than is allowed within the current restrictions of the NØNSAP code. We are, therefore, proceeding to reanalyze the model with a more refined model and the computer code STATIC-SAP.

5.4 REFERENCE

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6. PRIMARY SYSTEM MATERIALS TECHNOLOGY

P. L. Rittenhouse

This task provides a continuing assessment of the status of the technology of materials used for specific HTGR primary system components with the aim of identifying potential safety problems and/or possibilities for improved margins of safety. The subject assessment also supplies information useful as input to various systems and safety analyses.

6.1 ASSESSMENT OF METALLIC MATERIALS TECHNOLOGY FOR HTGR PRIMARY SYSTEM AND CORE SUPPORT STRUCTURES — P. L. Rittenhouse

Our assessments of the technology of metallic materials of HTGR components are presented in a report¹ published in December 1974 and in a second report² compiled in draft form in June 1975. These reports were prepared with format as follows. First, the components and their materials of construction were described. This included discussion of conditions (temperature, stress, and environment) in normal operation and during postulated accidents. Materials properties and behavior were related to these conditions, and potential areas of concern or uncertainty were identified.

The first report¹ covered the following components and materials:

1. class A and B thermal barriers (Grades A and B SA-285 low-carbon steels, Nitralloy 135M, Inconel 600, Hastelloy X);
2. main helium circulators (type 410 martensitic stainless steel, Inconel 718, type 422 stainless steel, 2 1/4 Cr-1 Mo steel);
3. steam generator (Incoloy alloy 800H, Class 2 SA-508 carbon steel, Grade A-1 SA-210 carbon steel, 2 1/4 Cr-1 Mo steel);
4. auxiliary cooling system (2 1/4 Cr-1 Mo steel, type 410 stainless steel, Incoloy alloy 800H, 1/2 Cr-1/2 Mo steel);
5. in-core components:
 - (a) flow control orifices (grade I SA-105 and grade A SA-106 carbon steels);

(b) top plenum elements (types 316, 321, and 347 stainless steels and 2 1/4 Cr-1 Mo steel);

(c) control rods (Incoloy alloy 800H).

In addition to discussion of these components and their materials of construction, separate sections were devoted to (1) the potential effects of the HTGR primary coolant environment (i.e., helium containing low levels of H₂, CO, CH₄, CO₂, and H₂O) on materials properties and behavior and (2) a description of ongoing programs relevant to HTGR materials technology and safety related areas. The information needs (i.e., potential areas of concern or uncertainty) identified earlier in the report were cross-referenced to existing programs to assess the coverage of information needs. Further, priorities were assigned to each of the needs by taking into account the importance of the component, the potential consequences of its failure, and the relative degree of deficiency of knowledge of relevant materials behavior. This procedure resulted in the selection of the five "highest priority" items listed below:

1. confirmation of the time-temperature conditions postulated to result in "damage" (a condition that requires repair before the resumption of operation and is defined as 2% total strain in the thermal barrier cover plates) and "failure" (a condition that could result in interference with core cooling, 4% total strain) strains in the Class A thermal barrier low-carbon steel cover plates;
2. as above for the Class B thermal barrier Hastelloy X cover plates;
3. evaluation of steam generator materials behavior under transient conditions such as could result from a loss-of-forced-coolant accident in which the steam generator isolation valves fail to close [These evaluations and analyses must reflect the effects, if any, of prior history (i.e., aging time and temperature in service, gas-metal reactions, corrosion, etc.) on materials properties.];

4. determination of the combined effects of irradiation and environment on the mechanical behavior of Incoloy alloy 800H control rod parts;* and
5. generation of materials design correlations (needed not only for design but for safety and performance evaluations) for low-carbon steels, Hastelloy X, Incoloy alloy 800H, and 1/2 Cr-1/2 Mo steel.

Follow-on work covering HTGR metallic materials and components resulted in considerable new information relative to HTGR primary system components. Several additional alloys applicable to these components were identified, assessed, and reported in the second report.² An assessment of the core lateral restraint structure and materials was also undertaken and reported in the same reference. A summary of these efforts — by HTGR components — is given below.

Thermal Barrier

Questions have arisen recently relative to the potential for corner-sag of the 1.07-m (42-in.) square low-carbon steel cover plates of the upper head Class A thermal barrier. Corner-sag is of concern because it could allow channeling of hot helium behind the thermal barrier. If a high probability of corner-sag is found under normal operation conditions or in over-temperature excursions, this could likely be compensated by redesign of the cover plate attachment fixtures or by selection of a stronger steel. A similar problem — with a possibly similar solution — may also exist with the Class B thermal barrier of the hot duct.

Main Helium Circulators

Two additional alloys, ASTM A 473 type 405 ferritic stainless steel and nickel-base alloy Inconel-X750, have been assessed relative to their use in the main helium circulators. We foresee no problems of safety significance associated with the use of these alloys.

* This item is discussed further in connection with work presented in Ref. 2.

Steam Generators

Considerable redesign effort on HTGR steam generators is in progress at GA. The major materials of construction (i.e., 2 1/4 Cr-1 Mo steel and Incoloy alloy 800H) are, however, expected to remain unchanged. In one application, the outer shroud with no steam exposure, ASME A 240 type 304 austenitic stainless steel is expected to replace Incoloy alloy 800H. Because of the high service temperature for this application [760°C (1400°F)], environmental effects (i.e., helium corrosion and possible consequent degradation of properties) should be considered in design and safety analyses.

In-Core Components

An update of information, relative to the first report,¹ on the design and materials of the plenum elements and flow control orifices has been provided.

Core Lateral Restraint Structure

The core lateral restraint structure is designed to provide lateral support for and to restrain the core and permanent side reflectors. It is composed of 252 support assemblies welded to the PCRV liner. Each of these assemblies consists of a pack of Inconel 718 springs housed in a grade 1 Incoloy alloy 800 pipe (see Fig. 6.1). A segmented core barrel constructed of SA-285 grade B steel plate is designed as part of the structure and functions as a gamma shield. No significant safety-related problems are evident in the use of the materials mentioned above, but further consideration should be given to possible changes in the properties of grade 1 Incoloy 800 and Inconel 718 as a result of thermal aging. Relaxation in the Inconel 718 springs should also be evaluated.

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Fig. 6.1. Core Lateral Restraint Structure.

6.2 ASSESSMENT OF MATERIALS IN HTGR REACTIVITY CONTROL AND SHIELDING COMPONENTS — C. S. Morgan, P. L. Rittenhouse, and R. G. Donnelly

Boronated graphite (i.e., graphite impregnated with boron carbide) is used in a number of reactivity control and shielding applications in the HTGR. These include the control rods, burnable poison rods, boronated shields, reserve shutdown system, and the upper plenum (top-reflector) elements. Our evaluations have included consideration of (1) degradation of boronated graphite by reaction with impurities in the primary coolant stream, (2) effects of radiation on the oxidation resistance and integrity of the boronated graphite, and (3) response of structural alloys (e.g., the Incoloy alloy 800H of the control rods) to radiation and to the boron plus carbon environment. The results of these assessments are summarized below. More detailed discussion is reported elsewhere.²

The B_4C and graphite making up the boronated graphite absorber compacts used in the various control components will oxidize in the presence of some of the impurities contained in the primary coolant helium. Since containers for the boronated graphite are vented, there will be access, but perhaps not unlimited, of the primary coolant to the absorber. Oxygen partial pressure will likely be so low as to preclude significant reaction and subsequent transport of boron by this mechanism. However, CO_2 and CO will be present in the helium coolant in measurable quantities [perhaps up to 45 Pa (450 μ atm) in the case of CO], and their effects on the stability of boronated graphite are unknown. Water vapor will oxidize boronated graphite to B_2O_3 and volatile boric acid. Although a number of factors may interact to decrease the rate of this reaction, our calculations — based necessarily on a number of assumptions — indicate that the resultant transport of boron from vented control rods is potentially of concern to both safety and operation.

Irradiation to a fluence of 2.5 to 5×10^{21} n/cm² is reported to result in dimensional changes of 1 to 2% in boronated graphite.^{3,4} Expansion volume is provided in the control rod absorber cans to accommodate such swelling.⁵ The thermal conductivity of boronated graphite is decreased by a factor of about 10 by neutron fluences on the order of above.⁶ The effects, if any, of the observed expansions on oxidation behavior or the thermal conductivity change on performance are unknown.

The Incoloy alloy 800H (control rod absorber cans and boronated shield cladding) and the type 316 stainless steel and 2 1/4 Cr-1 Mo steel (plenum elements) used to contain boronated graphite will be exposed both to radiation and a boron-carbon environment. Times (especially for the plenum elements and boronated shield cladding) and temperatures (especially for the control rod components) are sufficient to justify an assumption that boron and carbon will contaminate these alloys. Irradiation levels are such that we may expect damage only for the Incoloy alloy 800H of the control rod

cans. The degree of contamination of the boronated graphite container materials and the quantitative effect of this contamination on properties cannot be stated at present. However, small quantities of boron in steel can reduce ductility.⁷⁻⁹

6.3 ASSESSMENT OF CORE SUPPORT POST GRAPHITE — P. L. Rittenhouse

We have completed a preliminary assessment of core support post graphite. The posts are long columns of graphite, in groups of three, supporting the graphite core support block under each refueling region of the HTGR core. The rounded ends of the posts are fitted into spherical seats to accommodate differential horizontal movement between the core support blocks and the PCRV bottom liner. The upper spherical seats are of ATJ graphite and are recessed into the core support blocks; the lower seats (either ceramic or a low-thermal-conductivity graphite) are on ceramic bases located on the bottom head liner. A metal sleeve (presumably Incoloy alloy 800H), welded to the liner, encloses the ceramic base.

Our assessment was based on 0.15-m-diam (6-in.) posts of ATJ graphite as reported.⁵ More recent but informal information indicates a somewhat larger diameter [0.18–0.23 m (7–9 in.)] for the posts and a possibility that the graphite used will be something other than ATJ. However, the more important conclusions of our assessment are not significantly affected by the present uncertainty in design and material.

The design lifetime for the posts is 280,000 hr. Design temperatures⁵ for normal (full rated power) operation and worst faulted (DBDA* 5-min delay before startup of one CACS*) conditions are 770/1048°C (1418/1918°F) (nominal/maximum short-term) and 1010/1204°C (1850/2200°F), respectively. The mixed-mean coolant temperature at the core exit is just under 760°C (1400°F). The environment to which the posts are exposed is the primary coolant (helium containing low levels of H₂, CO, CH₄, H₂O, etc.) at the core exit. Fast neutron dose to the posts should be no more than 10²⁰ n/cm² during their lifetime.

* DBDA: design basis depressurization accident; CACS: core auxiliary cooling system.

The core support posts are loaded essentially in pure compression. However, any horizontal displacement between the core support blocks and the bottom head liner will cause the spherical ends of the posts to roll in their spherical sockets and, thereby, will result in movement of the contact area from the center toward the edge. This subjects the posts to an eccentric load effect. It is believed that the compressive load capacity of the posts is governed by local failure of the post ends (or spherical seats). Compressive load tests of a post and seat combination with a 51-mm (2-in.) offset (the maximum expected in any off-normal event) confirm this expectation (i.e., failure occurred in the post seat as a result of local hoop tension).⁵ Design of the posts is conservative, and failures as a result of primary compressive loads under normal conditions are not anticipated unless significant degradation of the graphite occurs during reactor lifetime.

Four primary concerns were postulated relative to the in-service behavior and degradation of properties of the core support post graphite. These were: (1) compatibility with ceramic and metallic components of the lower seat assembly, (2) irradiation damage, (3) fatigue and crack propagation behavior, and (4) oxidation by impurities in the primary coolant. The first concern relates to the possibility that reaction between the graphite and the lower spherical seat material (Si_3N_4 has been proposed in some designs) might lead to premature local failure of the post ends and/or seats. Similar concerns are applicable when the metallic sleeve contacts the bottom seat and insulators. Since designs are not yet final it is difficult to more fully assess this point. However, this question should be recognized and used as a criterion in design.

The fast neutron dose to the core support posts (10^{20} n/cm²) is not expected to result in significant dimensional or property changes to the graphite. However, irradiation might increase the graphite oxidation rate. No quantitative estimate of this potential effect is available at present.

The third concern mentioned above involves the possibility of failure of the core support posts by initiation and growth of cracks under both static and cyclic loads. Limited data on fatigue and crack growth in graphite precludes a reliable evaluation of the potential for failure by

these mechanisms. This subject is further complicated by possible effects of environment (oxidation and irradiation) on fatigue and crack growth behavior and the basic properties of the graphite. As an example, oxidation often beneficially affects (decreases) the propagation rate of cracks in materials by blunting the crack tips. Although not possible at present, fatigue and crack growth information should be considered in design and end-of-life assessments of core support post graphite.

Evaluation of the relationship between long-term graphite oxidation and strength loss of the graphite core support posts has also been pursued. (Details of the subject evaluation are presented in Chapter 3.) In summary, it was determined that, for core support posts operating at approximately 788°C (1450°F) under the technical specification limit of 10 vpm (ppm by volume) total oxygen, there is essentially zero probability that the end-of-life strength loss will exceed 50%. (Such a loss would correspond to a reduction in safety factor of from 10 to 5, the latter being the minimum specified at the end of life). In fact, even for total oxygen of 850 vpm, the probability of 50% strength loss is less than 0.1. However, if the maximum sustained temperature in any portion of the posts reaches about 871°C (1600°F), the probability of 50% strength loss at 10 vpm total oxygen is almost unity and the estimates of possible strength loss range up to 100%. Even though this latter result would be tempered by the larger diameter posts now being considered, it seems important that this whole area be studied in greater depth. Future assessments should involve definition and/or consideration of a number of factors, including the maximum sustained graphite temperature, corrosion environment and rate, and correlations between strength loss and burnoff.

6.4 REFERENCES

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7. SAFETY INSTRUMENTATION

J. L. Anderson, R. E. Battle, and M. J. Roberts

An ongoing study was initiated during the reporting period to assess the adequacy of and identify development requirements for safety instrumentation for large HTGRs. The object of the review is to identify areas where further development and design efforts would result in improved safety, reliability, or availability and is not related to licensing.

The base of information being used for the review comes from the *General Atomic Standard Safety Analysis Report (GASSAR-6)*¹ and from the Preliminary Safety Analysis Reports for the Summit² and Fulton³ plants. More detailed information comes from topical reports and published articles when available; in addition, comments from General Atomic Company (GA) also have been considered. Information from GA proprietary sources has not been included in this report. The study to date has been concentrated primarily on moisture monitoring instrumentation and the literature sources are noted as references.

7.1 REACTOR TRIP SYSTEM

7.1.1 Logic and Actuation

The proposed reactor shutdown system for large HTGRs is described¹ in *General Atomic Standard Safety Analysis Report (GASSAR-6)*. The shutdown system employs nine input parameters to each of three identical redundant channels. Four of these parameters are bypassed either automatically or manually during some conditions of reactor operations. The inputs of the three channels are arranged in two-out-of-three "general" coincidence. That is, a trip of one channel by any input parameter in coincidence with a trip of another channel by *any* input parameter in that channel will produce a trip. The coincident trip signals need not be measures of the same variables. This arrangement is quite acceptable, in fact preferred by many,

where the number of input parameters is reasonably low so that the probability of spurious trips due to multiple failures is acceptably low. The desirability of the general coincidence arrangement is attributed primarily to its simplicity and relative ease of testing. In the GASSAR-6 system,¹ unfortunately, this advantage of simplicity has been lost by subsequent conversion of the logic from straightforward two-out-of-three to a four-channel arrangement wherein pairs of channels are combined with one-out-of-two logic and the combined pairs are then arranged in two-out-of-two for the final output. A name that has been used for this arrangement is "one-out-of-two taken twice," which tends to divert attention from the two-out-of-two-final logic. On a probabilistic basis it can be and has been argued that the reliability of the one-out-of-two plus two-out-of-two arrangement is satisfactory.⁴ However, all such probabilistic arguments necessarily assume complete channel independence, which is never achieved in practice. In the GASSAR-6 system all three of the input channels are brought together and connected to the inputs of the two-out-of-three logic elements in all four of the subsequent logic channels. Not only are the channels brought together by the logic circuitry, but the wiring is necessarily intricate in these locations. It is not difficult to postulate wiring faults or semiconductor failures within a single logic circuit module that could result in the common failure of the entire system. Because of the special treatment usually provided for physical protection of shutdown system wiring and components, the probability of such a failure may be low. Nonetheless, the needless interconnection of supposed independent channels should be regarded with suspicion. The use of "one-out-of-two-twice" logic is common in power reactors, but the intermixing of logic schemes is not. The history of operating reactors clearly indicates that random component failures are not, and never have been, significant factors in the unreliability of protection systems. The vast majority of failures and incipient failures of protection

systems to perform their functions have been related to common-mode influences, interconnections and interactions between channels or systems that had been assumed independent. This experience suggests that more attention should be devoted to reducing common-mode failure susceptibility, rather than concentrating on random failures.

The output of the two-out-of-two logic system is a direct current, which supplies all the control rods in parallel. The current from each of the two parallel power supplies is normally switched both by an electronic switch and by a relay contact to reduce the probability of failure to scram; in addition, the control rod holding bus is shorted to ground. However, since all rods are fed in parallel, the potential for wiring faults at a strategic location still exists and could prevent all rods from scramming. The opportunity for wiring faults to power sources is considerable because of the way in which holding power and drive power are switched to the control rod drive motors. To move a control rod, two-phase ac power is applied to the windings. When not in motion, the dc current from the shutdown system output is applied to the same windings to "lock" the motor. When both the ac and the dc are removed the rod inserts by gravity with dynamic braking from the motor as determined by resistors permanently connected to the windings. The application of ac to all the motors would prevent scram. Examination of the drawings reveals appropriate interlocking to avoid interactions in the absence of faults, but fairly simple faults may result in the inability to scram promptly. A broader look suggests several opportunities for common failures that could result in failure to scram, such as: all rods in parallel for holding current, use of the same motor for both driving and holding, availability of different power sources to the motors, complex switching required because of the dual motor functions, two-out-of-two holding power source, multiple channel interconnections because of the mixture of logic schemes. In addition to these potential problems, the rod drives themselves

employ drum-and-cable mechanisms, which are prone to slack-cable entanglements and ambiguity of rod position information, and the rods are inserted by gravity alone with no mechanical assist for acceleration or to overcome friction. Drum-and-cable rod drives have a poor service history in reactor service at ORNL. They were used on a number of early pool-type research reactors and have mostly been replaced by gear or screw types with direct attachment to fuel assemblies to prevent inadvertent fuel movement. We believe other approaches should be developed for HTGRs.

A further potential degradation of channel independence may be provided by the Data Acquisition and Processing (DAP) System. The DAP system examines the status of all channels of the shutdown system at many points. Signal buffering is indicated, which will hopefully prevent faults in the DAP system from affecting all the shutdown system. Even though buffered, the presence of the many additional wires necessary for the DAP system increases the propensity for channel interdependence through noise coupling and fault potential. The accumulation of status information from many points within all channels of the protection system must be weighed against the greatly increased opportunity for common interaction through wire, cable, and noise communications among the channels. The need or desire for such extensive monitoring of the shutdown system may be artificially created by the unnecessarily complicated mixed logic of the system and the testing complications that this creates. An unfortunate trend is developing in all commercial reactor types to increase the role of the "plant computer" in protection system monitoring to the detriment of channel independence. A somewhat subtle influence is the use of a single, sophisticated plant computer to calibrate all the alleged independent protective channels. Such functional interdependence, with the inevitable degradation of protection system reliability, is apparently being abetted by the promiscuous use of electrical isolation devices. This trend toward diffusion of independence should be reversed.

Paragraph 4.7.2 of IEEE-279⁵ permits the use of isolation devices to transmit signals from protection system equipment for control system use. It is neither stated nor implied that such devices may be used to couple signals between alleged independent channels within the protection system. Paragraph 4.6 clearly states that channels shall be independent and physically separated. In this sense "independent" is intended to apply to functional performance and not just to potential failures through electrical interconnections. The use of electrical isolation devices does not give free license to functionally interconnect channels indiscriminately and thereby subvert the functional independence of the protection system.

7.1.2 Reactor Trip Inputs

A low-level neutron flux signal is used to protect against power excursions when the reactor is being operated at low levels for testing or refueling and is manually bypassed at high power with key switches on the control boards. Comments by GA on this state that: (1) the instrumentation used for this parameter is portable gear to be removed after initial core loading, and (2) the retractable in-core sensors and measurement channels are not used to provide an input to the reactor trip. This information is contradictory to paragraph 7.2.1.1.a.(1) of GASSAR-6.¹ We believe that the following comments are valid, whichever is correct.

The signals are derived from start-up pulse-counting channels, which will characteristically have slow response because of the statistical smoothing required and may be subject to paralyzing overload (foldover) because of pulse resolution limitations inherent to counting instruments. These characteristics must be carefully checked for appropriate transients during equipment qualification or earlier, as prudent. The in-core detectors (fission chambers) for this parameter are retractable and during high-power operation are normally withdrawn from the core with their

polarizing voltage removed. The detectors must be designed to operate at fairly high temperatures characteristic of the reactor core under the intended operating conditions. These channels may have to operate during fast restart conditions when the core temperature will be much higher than for low-power testing. If detector cooling is required for some modes of operation, then the cooling status must be a part of the protective function just as the polarizing voltage and retract status are as required by IEEE-279,⁵ should these channels be used for protection.

Generally, pulse counting instruments are undesirable for protective system use because of their characteristic unreliability and response limitations.⁶ This unreliability is due to the complex signal conditioning required, susceptibility to foldover or paralyzing overload conditions, and inherently slow response necessitated by statistical smoothing. If analysis really shows that protection at very low flux levels is required, then more reliable techniques for achieving this protection should be developed. In examining the protective requirements of reactors, it is usually found that the identification of a need for low-level flux protection is a direct consequence of insufficient speed of response of the shutdown mechanism. Equivalent or superior protection can usually be achieved with faster shutdown systems initiated at higher levels.⁶ Since gas-cooled reactors to date seem to be equipped with very slowly responding shutdown systems, we recommend that the entire subject of response requirements be thoroughly investigated and the needs and possible benefits of faster systems be explored. This same argument is applicable to light-water reactors.

The flux signal used in a flux-to-flow ratio input is the direct current from an ionization chamber, which is also used in pulse counting and variance modes for wide-range power indication. While such multiple use of the detectors may be acceptable, a couple of factors need to be considered in the evaluation. Firstly, the reliability of the protective signal may be reduced because

of the additional complexity of the circuitry required for multiple use. Should the variance signal be used for control (apparently not the case¹ for GASSAR-6), then a strong potential for undesirable safety and control interaction would exist. Secondly, and more significantly, in such a system the chamber location and characteristics will almost surely be optimized for the wide-range, nonsafety characteristics of the system. The dc output current used for protection will then be an accidental result of this optimization and may or may not be optimum or appropriate for protection. The locations of detectors for protective system use are usually carefully studied to assure that the neutron-to-gamma ratios and radiation levels are suitable and that the chamber sensitivities are adequate to provide proper response. This departure from usual practice indicated by the GASSAR-6 system will require careful scrutiny to assure that the protective system performance and reliability are not degraded by the use of protective system equipment for other purposes. In discussions on this subject, GA contends that the proposed detector locations are optimal for the HTGR. The economic incentive to use sensors and/or penetrations for multiple uses is considerable, and such multiple use has become fairly common, though still suspect, in protection systems. The development of the counting-Campbelling technique to commercial acceptance represents a significant advance in reactor start-up instrumentation. Nonetheless, the combined use of a detector for dc current, counting and Campbelling signals where the signals may be used for both protective and nonprotective functions represents a subtle and potentially significant degradation of both independence and performance of the safety function. The flux input to the flux-to-flow ratio channel is the most important of the flux-related protective signals since it is the only one operative during normal operation at full power. Therefore, this signal in particular should not be compromised for the benefit of others, even if the others are

also safety-related. A comprehensive study of the neutronic, geometric, and thermal requirements of ionization chambers for HTGRs is recommended.

A high-level neutron flux trip signal is effective during reactor start-up when helium flow is low. It is automatically bypassed by high helium flow. The signal is derived from the wide-range logarithmic channel that shares its detector with the flux-flow input described above.

Helium temperature is measured by thermocouples located in guide tubes at the steam generator inlet. The outputs of several thermocouples are combined to obtain the average gas temperature and to improve reliability. The adequacy of available thermocouples and other temperature sensors for HTGR applications is being reviewed by others (LASL HTGR Safety Research Program).

Based on the mixed results of this assessment and knowledge of other thermometry work in progress, the complete suitability of available temperature sensors for HTGR applications is not fully established. A comprehensive qualification and development program in this area is recommended to substantiate and support the work being done by GA. Our review in this area has not yet been detailed enough to identify specific deficiencies and development requirements that are based on identified HTGR needs. Temperature measurements at the core exit would be in a more adverse environment than those at the steam generator principally because of the higher neutron and gamma flux present.

Although it is not presently proposed to use the core outlet measurements in the shutdown system, such measurements are important to safety in detecting the presence of xenon oscillations, excessive fuel temperature, effectiveness of rod programming, power-flow mismatches, flux tilts and power imbalances, and adequacy of flow orificing. Because of a general lack of data on sensors under particular HTGR conditions, an accurate assessment would involve some substantial experimental investigation, which is beyond the scope of our current assessment.

The instrumentation for the remaining reactor trip inputs proposed is expected to be commercially available and adequate for HTGR applications except moisture monitoring, which is discussed at length later in this review. Qualification of commercial equipment to particular HTGR specifications will of course be necessary, but this is not expected to involve development.

7.2 CORE AUXILIARY COOLING SYSTEM (CACS)

During normal reactor shutdown, the main cooling loops remove the decay heat, but the CACS is required to remove reactor decay heat when the main cooling system cannot. The CACS may be initiated automatically or manually. The 1160-MW(e) HTGR as described¹ in GASSAR-6 has three cooling loops in the CACS, two of which must function correctly to remove the decay heat of the reactor following full-power operation. After the CACS receives a signal to start, a large amount of equipment must function to provide adequate cooling to the core; and, in general, the larger the number of items that must function the higher the probability of failure of the system. The actual number of items that must function for the CACS to provide adequate cooling is variable depending upon the initiating event and the condition of the reactor. Some portions of the equipment that are needed for CACS operation are functioning at all times during normal reactor operation, but certain pieces of equipment function only when tested or after CACS initiation. A general description of the CACS operation will assist in explaining the view that the auxiliary cooling reliability is limited by the large amount of equipment that must function for the CACS to remove decay heat. Most of the information described is taken from GASSAR-6, details of the Core Auxiliary Cooling Water System (CACWS) are taken from the Summit PSAR,² and some information comes from other sources.

During normal reactor operation, there is a small helium leakage through the auxiliary loop helium shutoff valve into the auxiliary cooling loop. The helium leakage allows parts of the CACS to function during normal reactor operation. It also avoids thermal shock by keeping the cooling water temperature close to the temperature at which it will operate when removing decay heat. The plant service water is also circulated through the service water heat exchanger during normal reactor operation. Heat is removed from the plant service water and exchanged to the atmosphere by an air-blast heat exchanger. The auxiliary Core Auxiliary Cooling Water (CACW) pump circulates cooling water during normal reactor operation, but it is not designed to remove decay heat from the reactor. Cooling water pressure is maintained within limits by a pressurizer with a helium cover gas. When the CACS is initiated, the main cooling loops must be shut down and blocked to prevent the helium flow from the auxiliary circulators from bypassing the reactor core. A six loop plant is designed to withstand the failure of one main helium shutoff valve. The 1160-MW(e) HTGR has six main cooling loops, and each loop has a large number of valves, which must function to shut down the loop. Five pairs of series redundant shutoff valves in each loop isolate the secondary coolant, and one valve with redundant actuation circuits blocks helium circulation in the main coolant loop. If auxiliary cooling is determined to be necessary, the logic system provides simultaneous signals to start the auxiliary helium circulators and to shut down the main cooling loops. Head pressure from the auxiliary circulator opens the helium shutoff valve in the auxiliary cooling loop and begins circulating helium through the core auxiliary heat exchanger (CAHE-helium) transferring heat from the helium to the cooling water. The speed of the helium circulator is controlled by a variable-frequency inverter, and the inverter frequency is controlled by the cooling water temperature out of the CAHE (helium). As the cooling water temperature increases, the frequency of the inverter is made to decrease, thereby decreasing

the speed of the drive motor with a resultant decrease in water temperature. The cooling water temperature at the inlet to the auxiliary heat exchanger (helium) is maintained within limits by controlling the cooling water temperature out of the CAHE (service water). The CAHE (service water) transfers heat from the cooling water to the service water. The CAHE (helium) cooling water inlet temperature is controlled by a motor driven valve, which regulates the amount of cooling water flow through and around the CAHE (service water). The helium circulator drive motor is serviced by a lubrication system, a component cooling system, and a helium buffer system. The lubrication system and the component cooling system are necessary for drive motor operation; the helium buffer gas is not necessary for safety, but only to prevent lubrication vapors from contaminating the helium coolant. Operation of the auxiliary helium circulators increases the helium flow through the auxiliary heat exchangers and increases the cooling water temperature. When the CACS is initiated, the CACWS is switched from the standby cooling mode to the core cooling mode automatically. When the CACS goes into the core cooling mode, the main CACW pump is started, the auxiliary CACW is stopped and its bypass valve closed, and the water purification system is isolated. Cooling water flow back through the auxiliary CACW pump is prevented by a check valve. A detector in each of the main cooling loops and in each of the auxiliary cooling loops detects moisture so that action can be taken to avert a steam-graphite reaction. The main steam generator isolation and dump system must function upon demand of the main loop moisture monitoring system, and likewise, the auxiliary isolation system must function when required by the auxiliary moisture monitoring system. The loop isolation systems are designed to isolate a maximum of two main loops or one auxiliary loop upon the detection of excessive moisture, so adequate cooling will remain. If the auxiliary cooling water level becomes too low or too high, the cooling water makeup system or the water vent may have to function.

The above paragraph states, in general, how the core auxiliary cooling system works. The paragraph reveals that much equipment must function properly for removal of decay heat by the auxiliary cooling system. Major simplification of the system may not be practical, but any possible reduction in complexity should result in higher reliability.

Some equipment in the CACS appears particularly susceptible to a failure, which may result in hazardous conditions. Two specific items that we have recognized as possibly needing improvement are the primary helium shutoff valve and the auxiliary circulator drive motor lubrication system.

The primary coolant shutoff valve is necessary to prevent the helium flow produced by the auxiliary helium circulators from bypassing the reactor core and decreasing the core cooling by flowing through the main helium circulators. Each of the six main loops has one helium shutoff valve. The logic system that actuates the primary helium shutoff valve is divided into logic A and logic B. Logic A provides a signal to three drive motors, any one of which can close the valve. The motors do not have to be synchronized and will not jam each other since they are driven through slip clutches. Logic B signals a solenoid to let the valve close by gravity with a spring assist. Limit switches turn on a light to indicate that the valve is closed. Because of the two methods of closure, failure to actuate the valve is unlikely. However, since there is only one valve by some method the valve might not seat properly. For example, an obstruction or valve warpage may prevent the valve from closing but still allow helium to flow. Section 5.2.1.7(b) of GASSAR-6 indicates¹ that the CACS can provide adequate cooling of the core even though one of the primary coolant shutoff valves has failed open. The reliability of the helium shutoff system might be improved by using redundant valves. Conversely, inadvertent blockage of helium flow through

the loop is more likely if two helium shutoff valves are used rather than one. Although the reliability of the helium shutoff system would be improved, the reliability of the main cooling loops to provide core cooling would be reduced if two helium shutoff valves were used.

Lubricating oil is supplied to the auxiliary helium circulator motor by two oil tanks. One tank supplies oil to the upper bearing cavity, and the other to the lower bearing cavity. Initially, a predetermined quantity of oil is supplied to each cavity; then the oil supply valves are closed isolating the bearing cavities from the oil tanks. No instrumentation to indicate oil level in the motor is mentioned¹ in GASSAR-6, although recent information from GA indicates that oil level monitoring will be provided. Loss of oil may go unnoticed and result in motor damage and loss of the auxiliary circulator. At a labyrinth seal between the helium coolant and the bearing cavity, buffer helium is injected to prevent oil vapors from getting into the primary coolant. A possible method of failure of the motor lubrication system is summarized here. If the helium coolant suddenly changes pressure, the labyrinth seal may fail or oil may be drawn through the seal by the pressure change. If the change in oil level is not observed by the operator, and if the oil loss is sufficient the motors could be damaged. Failure of the labyrinth seal by this method can result in common mode failure of the auxiliary cooling loops.

7.3 STEAM GENERATOR ISOLATION AND DUMP SYSTEM

The steam generator isolation and dump system consists of the main loop shutdown system and steam generator dump system. The primary helium shutoff valve, the superheater-evaporator-economizer isolation valves, and the reheater isolation valves are the active components in the main loop shutdown system; and, except for the primary helium shutoff valve, all the other main loop shutdown valves are redundant. The steam generator dump system consists of a dump tank and parallel dump valves, which are operated by

two independent hydraulic systems. One dump valve is actuated by a signal from logic A, and the other is actuated by a signal from logic B. In series with the dump valves, however, are manually operated dump block valves, which are closed when the dump system is being tested. By closing the dump-block valves, the system can be tested without actually dumping the steam generator. A common mode failure of the steam generator dump valves is realized if the two dump-block valves are left closed when the dump test is completed; and if only one dump-block valve is left closed, then the dump system is subject to subsequent complete failure by a single failure of the remaining functional valve. The dump-block valves are not instrumented to indicate whether they are closed. Indication should be provided to the operator whenever the dump-block valves are not completely open. It is important to observe that if one or both of the dump-block valves are left closed they will probably remain closed until the next dump system test, eliminating the increased system reliability gained from redundant valves. Providing dump-block valve position indication to the operator will increase the dump system reliability.

7.4 CAHE (HELIUM) ISOLATION SYSTEM

GASSAR-6 delineates¹ the general procedure to be followed if one of the auxiliary cooling loops is temporarily out of operation but is incomplete. With the reactor at full power and one of the core auxiliary cooling loops out of service, the reactor will have only 100% of its auxiliary cooling capacity instead of 150%. In this mode of operation, a single failure in the auxiliary cooling loop will result in insufficient auxiliary cooling capacity. The status of the auxiliary heat exchanger isolation system must be examined for reduced auxiliary cooling capacity. If the CACS loop isolation system is locked out when one auxiliary cooling loop is down, and the CACS is required to cool the core, a moisture leak in an auxiliary loop can cause a damaging graphite-water reaction. When the CACS is required to cool the core, and the isolation system is not locked out, a moisture leak will shut down one of the two remaining loops, leaving only 50% of the necessary

cooling capacity, and the core may overheat. For extended downtime of one of the core auxiliary cooling loops, the core auxiliary cooling loop isolation system should not be locked out, and the reactor will probably be required by technical specifications to operate at reduced power so the reactor decay heat does not exceed the decay heat removal capacity of a single auxiliary cooling loop. (Reference GASSAR-6, paragraph 16.4.3.3.)

7.5 ORIFICE CONTROL SYSTEM

The orifice control system is used to control the temperature profile of the reactor core by varying the helium flow through 73 regions within the core. The orifice control system is not required for safety. Manually controlled, motor-driven valves regulate the helium flow within each region. The orifice valves can only be operated one at a time in incremental steps with a time delay between successive steps. Redundant thermocouples measure the temperature of helium leaving each region, and the data acquisition and processing system (DAP) provides the operator with a cathode-ray tube (CRT) display of the helium temperature in each region. Each valve also has a position transmitter, which provides a signal to the DAP for display on the CRT. High temperature, low temperature, and temperature deviation between the two sensors are alarmed. When the reactor is down for refueling, the calibration of the thermocouples and the DAP is verified by use of precision thermocouple potentiometers. Since the instrumentation is redundant, excessive temperature at the outlet of a region will likely be indicated to the operator. However, the control system is not redundant, and a single failure in the control system can result in loss of control of all the orifice valves. If loss of control of the valves results in excessive temperature in a region, the operator can reduce the temperature by inserting the control rod pair in that region.

Redundant thermocouples and an alarm system provide the operator with indication of excessive temperature within the core regions, and a stepping-motor valve-control system and control rods provide control of the temperature profile of the core. Control of the temperature profile of the core depends strictly on correct operator response to the information provided by the temperature indicators. The effects of an incorrect response have yet to be determined.

7.6 MOISTURE DETECTION IN HTGRs: PRESENT STATUS AND DEVELOPMENT NEEDS

7.6.1 Requirements

The HTGR employs helium at 5.00 MPa (725 psia) as the primary coolant to transfer heat from the reactor core to the steam generators.⁷ As pressures of up to 17.2 MPa gage (2500 psig) are reached on the steam side of the steam generators, a leak in the steam-helium boundary could allow moisture to enter the primary coolant. Excessive moisture in the hot graphite of the core could increase operating pressure, erode the graphite moderator, and liberate hydrogen and carbon monoxide. The possibility of these adverse effects of moisture on the graphite fuel elements requires instrumentation for the rapid detection of moisture in the helium coolant loops.⁸ This system must provide a reliable means of detecting moisture at all normal and abnormal operating conditions. It should also identify the particular loop with the excessive moisture so that it can be isolated and the steam generator dumped.

7.6.2 Operating Experience in Detecting Steam Generator Leaks in HTGRs

This section will summarize the experience with electrolytic hygrometers and infrared analyzers at Peach Bottom and in England and the experience with thermal conductivity and variable-capacitance devices at Peach Bottom. It will also cover the experience to date

at Fort St. Vrain with optical dew-point devices. To complete the assessment of the state of moisture detection, operating experience in France, Germany, and the Soviet Union should be reviewed.

During the period 1968 through 1972, electrolytic hygrometers made by Beckman were used at Peach Bottom⁸ for moisture detection. Experience accumulated during their use indicated a mean usable life of about six weeks. The electrolytic hygrometer cell consists of two rhodium or platinum wires wound in parallel helices spaced a short distance apart on the inner surface of a plastic cylinder. The inner surface of the cylinder is wetted with phosphoric acid. A dc potential applied to the wires electrolyzes the water content of the acid leaving a desiccant with a high affinity for water. As moist gas passes through the tube the dessiccant absorbs the moisture and a dc current flows to electrolyze it. The humidity of the gas is then proportional to this current. The failure of these cells in long exposure to moist gas is in the upscale direction, apparently due to the migration of metallic compounds formed during electrolysis from one electrode to the other, eventually bridging the gap and electrically shorting the cell. The failure mode in long exposure to very dry gas is in the slightly downscale mode, apparently due to cell dehydration.⁹ The British experience with electrolytic hygrometers described by Bennett and Conibear¹⁰ has been similar to the Peach Bottom experience with a few minor differences. They also have had problems with cell degradation and failure. The life data are hard to compare since their coolant is carbon dioxide instead of helium and the typical moisture concentrations for which they specify life of cell are different from those reported by Peach Bottom. Bennett and Conibear do not mention degradation of the cells by exposure to very dry gas but dwell extensively on the problems associated with long exposure to high moisture concentrations. By using a low-frequency ac driving voltage and adding cadmium to the electrolyte, they have effectively anodically protected the

wires from the effects of exposure to moisture, thereby increasing cell life. Also they have developed techniques for regenerating failed cells with a success rate of about 60%. They have also developed a differential measurement technique, which eliminates problems associated with drifts in background current at zero moisture.

The British have also used Infrared Gas Analyzers (IRGA) for moisture detection.¹⁰ The performance is reported as being affected by drafts, temperature, gas pressure, and reactor power, and drift may be 5 to 10%/day. The equipment requires very frequent checking, and the maintenance costs are relatively high. The IRGA is operationally satisfactory as a trend indicator with an overall response time of about 5 min. The Peach Bottom test program^{7,11} with infrared analyzers showed an rms response time of 9.2 sec and limited sensitivity compared with other methods. The sensitivity could be increased, but only at the expense of response time.

The thermal conductivity bridge designed by GA is a trip device only. The Peach Bottom test program^{7,11} showed it to have a response time (rms) of 1.3 sec. It is very sensitive to flow and pressure and cannot be used as a continuous indicator. The aluminum oxide variable capacitance device was unacceptable because of major shifts in calibration and lack of repeatability.^{7,11}

The ruggedized optical dew-point device^{7,11,12} used by GA at Fort St. Vrain can be used in the indicating or trip mode but not both simultaneously. It offers flexibility, a high accuracy, and rapid response in the trip mode. It is comparatively slow in the indicating mode. It is also very complex in that it requires support systems such as a liquid-nitrogen cooler or Freon refrigerator if measurements as low as 1 vol ppm are required.

7.6.3 Comparison Among Methods of Hygrometry

7.6.3.1 Electrolytic Hygrometer^{7-11,13}

The principle of operation is described in Section 7.6.2. The accuracy of the electrolytic hygrometer is limited only by the regulation of the sample (mass) flow through the cell and the precision of cell output-circuit components. As previously stated, its operating success has been mixed with a too frequent failure rate. The references (British and American) paint somewhat different pictures of its usefulness, with the British seemingly the more optimistic about its ultimate success. It has the advantages of relatively low cell replacement cost, uncomplicated electronics and supporting hardware, and direct measurement of moisture with the sensitivity and response time required. Its disadvantage is its susceptibility to frequent failure in very moist or very dry helium.

7.6.3.2 Optical Dew-point Hygrometer^{7,8,11-13}

The optical dew-point hygrometer contains a cooled mirror, an optical bridge, a temperature sensor, and readout electronics. In the indicating mode the sample gas flows across the mirror. The mirror is cooled until liquid or frost forms on it. The optical bridge then senses a change in reflectance and develops a feedback signal to control the mirror temperature at the dew or frost point. This temperature indicates the moisture concentration of the gas. In the trip mode, the mirror temperature is held constant at a dew or frost point corresponding to the moisture concentration of the gas at which a trip should occur. When the moisture concentration reaches that level, the mirror fogs and a trip signal is generated. Its speed of response and accuracy are sufficient to meet specifications as a trip instrument. Used in the indicating mode, its sensitivity and accuracy are also sufficient to meet specifications.

7.6.3.3 Infrared Detectors^{7,8,11,13-15}

Infrared detectors typically contain an infrared source, an infrared filter, a sample cell, an infrared detector, and associated electronics. The infrared beam is filtered to select a wavelength that will be absorbed by moisture but not by helium. This filtered beam is passed through the sample gas. Another filtered beam whose wavelength is selected to pass unattenuated through both moisture and helium is passed through the sample gas. Both beams are measured after passage through the gas. Their ratio is functionally related to the moisture concentration. This type analyzer, if built to have adequately fast response, has inadequate sensitivity and vice-versa. It also suffers from drift problems and complicated hardware, which limit its use to trend indication.

7.6.3.4 Microwave Methods^{8,16}

Two moisture detection methods use microwave methods. Water vapor absorbs microwave energy at 23 GHz during passage through a sample of coolant gas. This absorption due to water vapor is hard to measure. A more promising technique is to use the principle that water vapor changes the dielectric constant of the gas. The measurement technique is then to detect the change in resonant frequency in a gas-filled cavity as the gas changes from being dry to moist. This technique is sensitive to pressure and temperature. Therefore the sample gas cavity resonant frequency should be constantly compared with the resonant frequency of a similar cavity at the same pressure and temperature, which also samples the coolant but at a much slower rate. Consequently, one frequency shift would occur more rapidly than the other, and the sudden change would be detected by a microwave bridge imbalance. Shifts in resonant frequency due to temperature and pressure changes would be essentially the same for both samples and would cancel in the bridge. According to Billeter and Schemmel,¹⁶ a microwave device could accurately detect over a long period of time moisture

changes as small as 12 vol ppm. Typical response time is approximately 0.6 sec. The minimum discernable concentration change (short-term) is approximately 2 vol ppm. An instrument of this type could be placed directly in the boiler exit pipe, thereby eliminating sample transport lag. Two disadvantages of this method are the complicated hardware and electronics and the lack of development for HTGR application.

7.6.3.5 Thermal Conductivity Sensor^{7,8,11,13}

As designed by GA, this unit consists of a four-element bridge, a delay line, and power supply and detector electronics. Four commercially available hot-wire elements form the electrical bridge. The sample gas passes over two opposite elements in the bridge, through a coiled-tube delay line, and later through the other two opposite elements in the bridge. The electrical resistances of the hot-wire elements change as the thermal conductivity of the sample gas, which is a function of moisture concentration, changes. Moist gas passing through the first set of elements creates an imbalance in the bridge. Passing through the second set restores the balance. This instrument can be used for trip only, not for indication of moisture concentration. It has a response time of 1.3 sec (rms). It is quite sensitive to flow and pressure.

7.6.3.6 Nuclear Magnetic Resonance (NMR)

Detection of water vapor depends upon measurement of the resonance absorption of protons in the water molecules when the water enters the helium stream. It is suggested that the nuclear induction method can be used in which a gas sample is continuously passed through a sample cell surrounded by a magnetic field. The main advantage of NMR is that it is a positive indication of water only; fluctuations of temperature or pressure would not produce an error signal. The main disadvantage is that it has not been developed for HTGR application.

7.6.3.7 Helium Afterglow Monitor¹⁷

This technique is based on the detection of the characteristic radiation resulting from Penning-type ionization reactions between metastable helium atoms and impurities, in this case water vapor. Under laboratory conditions this method is very sensitive; it can detect water vapor concentrations in the volume parts per billion range. This sensitivity will be reduced somewhat by the contaminants in an HTGR, but it is still probably in the low volume parts per million range. The disadvantages are the complex equipment needed, the sensitivity to pressure and temperature, and the lack of operational experience in an HTGR environment.

7.6.3.8 Other Methods

Several other methods, hygrometric and otherwise, have been proposed for detecting boiler leaks. The variable capacitance sensor^{7-9,11,13} and the Brady array¹⁸ have both been shown to have the problem of decalibration after long use. The speed of response of the variable capacitance sensor also decreases greatly with extended use. Measurement of carbon monoxide, a product of the water-graphite reaction, can detect leaks but only after the reaction has occurred, making the response time too long. Also, it cannot identify the leaking boiler.

All the following are proven laboratory-hygrometric methods, but are untried in an HTGR environment to the best of our present knowledge:

1. pneumatic bridge hygrometer¹⁹ (complex, temperature-sensitive);
2. ionic salt-phase transition^{13,20} hygrometer (complex, requires careful filtering of particulates from the gas),
3. piezoelectric sorption hygrometer^{13,17} (temperature and pressure sensitive, must filter the gas, no known hygroscopic coating material satisfied HTGR requirements),
4. alpha-ray absorption¹³ (sensitive to radiation),
5. 6.13-MeV-gamma-ray detection¹⁷ (insensitive to low-level humidity because of background radiation).

7.6.4 Summary and Recommendations for Developmental Work

Based on all the information gathered thus far, the authors' opinions are as follows:

1. No complete moisture detection system has conclusively proven its acceptability in an HTGR environment. The optical dew-point hygrometers at Fort St. Vrain²¹ promise the best performance thus far and have undergone extensive developmental testing in service during the hot flow testing and start-up period. The performance of the GA moisture monitor detection system has met the design criteria, but problems have been experienced with the sample system. These problems have been primarily associated with drawing a continuous sample at all power levels since the sample is derived from circulator pressure drop. These problems should not occur in the GASSAR design since a sampling system consisting of heat exchangers, storage tanks, and compressors will be provided to ensure continuous sampling. According to GA, the lack of an alarm during moisture ingress at Fort St. Vrain was attributed to the monitors being set at a trip point above the dew point of the sample line coming through the PCRV. The setting was 10 times higher than the proper setting and the moisture condensed in the sample lines before the moisture level reached the set point of the monitor.

2. The two most promising techniques, considering all factors, are optical dewpoint hygrometry and electrolytic hygrometry.

3. There is an appearance of insufficient communication and crosschecking of results between United States investigators and foreign sources, especially France and Germany.

4. For all practical purposes there is only one United States research and development effort, the one at General Atomic Company, into moisture monitors for HTGR application. There has been a small effort into radiation methods at Los Alamos and into infrared detection at Battelle Northwest Laboratories. In the interest of objectivity, competitive independent investigation should be made to cross-check GA's conclusions.

5. Development work is needed in the area of nuclear magnetic resonance and helium afterglow detection methods. These are promising laboratory techniques, untried in an HTGR environment.

6. A response from GA indicates that if other moisture monitoring methods are investigated, development should concentrate on the pneumatic bridge and thermal conductivity probes, with the belief that the pneumatic bridge is less complex than the laboratory techniques of nuclear magnetic resonance and helium afterglow detection. GA points out that when the pneumatic bridge is used at high pressure, such as would exist in the HTGR sample system, the differential pressure is much higher and falls in the range of standard process instrumentation differential pressure transmitters. Also, since sonic nozzles are used in the pneumatic bridge, the pressure and flow conditions would be stable and unaffected by upstream or downstream pressure and flow changes. This stable condition should be ideal for the thermal conductivity probes, which are fast and simple devices. GA points out that the temperature sensitivity of the pneumatic bridge is of concern only when the temperature of the gas in one side of the bridge is different from that of the opposite side. These two gas temperatures could be maintained the same by a simple, effective heat exchanger.

7.7 BRIEF SUMMARY OF CONCLUSIONS AND RECOMMENDATIONS

7.7.1 Reactor Trip System

1. The mixed-logic arrangement of the protection system complicates testing and increases the opportunity for interaction between channels. At the point of logic mixing all channels are interconnected by the logic circuitry, and the wiring for the supposed independent channels is necessarily very close and susceptible to wiring faults. A straightforward logic scheme that would maintain better channel separation would be preferred.

2. The dc holding power source for the control rods uses two-out-of-two logic, and all rods are powered in parallel. The ac rod driving power is applied at the same point through a complicated switching arrangement. This arrangement appears to have a high susceptibility to common-mode failure through power or wiring faults and defeats the redundancy of multiple rods by powering all in parallel. It is recommended that the rod driving and the scram release circuitry and mechanisms be made independent of each other and that the scram release circuitry be rearranged to provide true redundancy.

3. It is recommended that development be initiated to produce a rod drive for HTGR service that is more suitable and reliable than the drum and cable drives being used.

4. It is recommended that the requirements for monitoring in the protection system be reevaluated so that the intrusions into the system by the data acquisition system might be minimized. The need for such extensive monitoring could be greatly reduced by the use of a simplified logic arrangement and a more effective on-line testing scheme.

5. It is recommended that pulse counting instruments not be used in protection circuits because of their inherently poor time response, complexity, and paralytic overload characteristics.

6. The need for low-level flux trips should be reevaluated and substantiated and the needs and benefits of a faster responding shutdown system explored.

7. It is recommended that detector characteristics and locations be optimized for protective features rather than for control and monitoring functions. The multiple use of detectors should not compromise their characteristics for protective system function nor degrade their reliability with complex interconnections with control and monitoring circuitry.

8. A comprehensive qualification and development program for core-outlet and other high-temperature sensors is recommended to substantiate and support the work being done by GA.

7.7.2 Core Auxiliary Cooling System

1. A large amount of equipment must function to provide emergency core cooling when the main circulators are not operating. The possibility of reducing system complexity should be explored.

2. The use of redundant helium shut-off valves in the main loops should be considered.

3. Lubrication monitoring of the auxiliary helium circulator motors should be provided, and the potential for common failure of the labyrinth seals, which may lead to circulator failure, should be investigated.

7.7.3 Steam Generator Isolation and Dump System

Misoperation of the steam generator dump-block valves (during testing, for example) may leave the system in an undetected vulnerable condition. Dump-block valve position indication and/or interlocking should be provided.

7.7.4 Core Auxiliary Heat Exchanger Isolation System

Reactor operating limitations or contingency provisions with one auxiliary cooling loop out of service need to be completely specified. In this situation the automatic provisions for loop isolation upon moisture ingress may result either in inadequate afterheat cooling or in moisture damage if isolation is inhibited.

7.7.5 Moisture Detection in HTGRs

1. No complete moisture detection system has conclusively proven its acceptability in an HTGR environment, although an optical dew-point system has been qualified at Fort St. Vrain. The most promising techniques are optical dew-point hygrometry and electrolytic hygrometry.

2. Communication and cross-checking of results should be improved between U.S. and foreign investigators.

3. Competitive independent investigation and development of moisture monitoring techniques should be initiated to supplement and verify the work at GA. Development work is needed in promising laboratory techniques of nuclear magnetic resonance and helium afterflow detection.

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PART II. HTGR SAFETY STUDIES FOR THE GAS-COOLED REACTORS
BRANCH OF THE DIVISION OF REACTOR LICENSING, USNRC

J. P. Sanders, Program Manager

This was part of a continuing effort for the Gas-Cooled Reactor Branch (GCRB) of the Directorate of Licensing (DOL). At the beginning of the reporting period, efforts were directed toward the review of the licensing topical report¹ (GA-LTR-1) on HTGR core cooling capability. Specific aspects of the Preliminary Safety Analysis Report² (PSAR) for the Summit Power Station were evaluated. In particular, attention was given to the performance of the thermal barrier cover plates on the PCRV liner at the indicated limiting temperatures and to the heat removal capacity of the auxiliary heat exchangers. Calculations were also performed to determine the power requirements of the auxiliary helium circulators. Efforts were then directed to similar evaluations relative to the Fulton Generating Station³ of the Philadelphia Electric Company.

Computational methods were developed to determine the thermal transient following a coincidental reactor trip and loss of both coolant flow and pressure. Ultimately, a three-dimensional R0Z model was used in which the transient fuel temperatures were determined for the various regions having specified fuel ages (total irradiations). This relationship was important since the fuel failure temperature was a function of exposure to irradiation. Parameter studies were made to relate the three-dimensional calculations with a two-dimensional model that could be executed with reduced computer requirements.

¹V. Joksimovic et al., *An Analysis of HTGR Core Cooling Capability*, Gulf-GA-A12504 (GA-LTR-1) (March 30, 1973).

²Delmarva Power and Light Company, *Summit Power Station, Preliminary Safety Analysis Report*, DOCKET 50-450.

³Philadelphia Electric Company, *Fulton Generating Station, Preliminary Safety Analysis Report*, DOCKET 50-463.

The performance calculations for the auxiliary heat exchanger and circulator were combined, and the effect of air ingress into the primary loop following a depressurization accident was investigated. An independent computational method was also formulated to estimate the rate of air ingress from the containment building during this interval.

In January 1975, the U.S. Nuclear Regulatory Commission (NRC) was created, and this work continued under the same branch, which was now a part of the Division of Reactor Licensing (DRL). At the request of the sponsoring branch, the fuel failure criteria presented graphically in Figs. C.3-4 and C.3-5 (Change 8) of the Fulton PSAR³ was reviewed.

A task relating to the operation of the Fort St. Vrain reactor under emergency conditions was assigned. This was a result of a request for a variance in a previously issued technical specification; the variance requested a change in the speed of the auxiliary Pelton-wheel drives for the helium circulator from 10,550 to 7000 rpm. The major points of interest were the maximum temperature in the core and on the surface of the thermal barrier cover plates during a particular depressurization accident.

8. ANALYSIS OF LOSS OF FORCED CONVECTIVE COOLING ACCIDENT USING THE HEATUP CODE

W. D. Turner

ORNL is charged with providing technical assistance to the Gas-Cooled Reactors Branch (GCRB) of the Directorate of Licensing (DOL) in the safety evaluation of the transient response of active cores of HTGRs under abnormal operating conditions. It was of importance to obtain the transient temperature distribution following a loss of forced circulation (LOFC) accident, which was recognized as one that would lead to a maximum hypothetical fission product release (MHFPR). Thus, the HEATUP computer code was especially designed to study the transient accumulation of heat in the active core of an HTGR for the analysis of the MHFPR arising from a total LOFC accident and simultaneous reactor trip. It was specifically designed to calculate the maximum temperature as a function of time as well as the fractions of active core whose temperatures exceeded specified values as a function of the age of the fuel in the various refueling regions. It was used in the thermal analysis of LOFC accidents involving both the Summit Power Station and the Fulton Generating Station.

8.1 DEVELOPMENT OF THE HEATUP COMPUTER CODE — I. I. Siman-Tov and W. D. Turner

The HEATUP code, a modification of the general, multidimensional computer program¹ HEATING3, was designed for the thermal analysis of a loss of forced circulation (LOFC) accident in a High-Temperature Gas-Cooled Reactor (HTGR). The code is designed to calculate the transient temperature distribution for two-dimensional *RZ* and three-dimensional *RθZ* models of the active core of an HTGR after a LOFC accident. The calculations assume no coolant flow in the active core during the transient. The thermal properties may depend on temperature and position. The effective thermal conductivity may also be anisotropic in the active core, and this function is calculated from input data concerning the fuel, graphite, and coolant.

The power in the reactor is a function of the radial and axial power peaking factors as well as time. The code HEATUP is designed to calculate the percentages of active core whose temperatures exceed three criteria, which are functions of the time the fuel has been in the reactor (or fuel age). If the respective temperature criteria for each age group represent the same condition of the fuel, then the code can compute the total percentage of active core whose temperature exceeds the criterion describing the same condition of fuel in all age groups.

A draft of a user's manual for the HEATUP computer code has been completed.² The user's manual will include a description of the basic problem; a review of results related to the choice of thermal properties, boundary conditions, and initial conditions; a full description of the three-dimensional *RθZ* model and a limited one of the two-dimensional *RZ* model; the special computations that were added to HEATING3 in the development of HEATUP; input preparation; and a sample problem consisting of the three-dimensional model of the Fulton Generating Station LOFC accident.

8.2 THERMAL ANALYSIS OF LOFC ACCIDENT INVOLVING SUMMIT POWER STATION — I. I. Siman-Tov and W. D. Turner

The HEATUP Computer Code was used in the thermal analysis of an LOFC accident involving the Summit Power Station HTGR. The models used, the details of the calculation method, and the results are presented elsewhere.³ At 17.6 hr after an LOFC accident, the calculated maximum core temperature was 3340°C (6050°F) for a model of the hottest fourth of the core and 3220°C (5830°F) for a model of a nominal third of the core. The time-dependent percentages of active core whose temperatures exceeded the specified temperatures of 1020, 1205, and 1390°C (1868, 2201, and 2534°F) are depicted in Figs. 8.1 and 8.2 for the two models. Again the fourth-of-core model produced a more rapid development of temperatures. Also, the radial power peaking factors in the outer refueling regions of the fourth-of-core model were greater than those for the third-of-core model.

Fig. 8.1. Percentage of Active Core Above Specified Temperatures for the Fourth-of-Core Model Following LOFC Accident, Summit Power Station. Temperatures are 1020, 1205, and 1390°C.

Fig. 8.2. Percentage of Active Core Above Specified Temperatures for the Third-of-Core Model Following LOFC Accident, Summit Power Station. Temperatures are 1020, 1205, and 1390°C.

The power distribution used in this calculation reflected values calculated at some point between the middle and end of cycle for an equilibrium core. At the beginning of cycle, peaking factors may be slightly larger; however, the total fission product inventory will be less. The point in cycle that produces the greatest MHFPR has not been investigated since this would require a series of calculations involving the nuclear physics calculations and a detailed description of the operating history of the core. Variations due to perturbations in power density over core cycle time are not anticipated to affect the results to a greater degree than some of the simplifying assumptions included in this calculation.

8.3 THERMAL ANALYSIS OF LOFC ACCIDENT INVOLVING FULTON GENERATING STATION — I. I. Siman-Tov and W. D. Turner

A three-dimensional *RθZ* model similar to the fourth-of-core model developed for the Summit Power Station (Sec. 8.2) was used for the thermal analysis of the LOFC accident for the Fulton Generating Station. In addition, a two-dimensional *RZ* model was used, and temperature transients calculated with these two models were compared. The models, assumptions, and detailed results are presented elsewhere.⁴

8.3.1 Results of Computations

The two-dimensional model gave a maximum core temperature reaching 4150°C (7500°F) in 23.6 hr. It gave a steeper rise than the three-dimensional model for short times but then a slower rise. The rise curves for the two models crossed at about 0.8 hr and 1270°C (2315°F), reaching 2070°C (3750°F) in 4.7 hr for the three-dimensional model and 5.0 hr for the two-dimensional model. The portion of the core exceeding certain temperatures was more sensitive to the model than was the maximum temperature, particularly for the first 2 hr after the accident, as shown in Fig. 8.3.

Fig. 8.3. Comparison of Percentage of Active Core Above Specified Temperatures for Three- and Two-Dimensional Models Following LOFC Accident, Fulton Generating Station. Temperatures are 1020, 1205, and 1390°C.

The report⁴ also compares the maximum temperatures of the two-dimensional model with results for the same accident conditions but a different model.

The analyses performed for the Fulton Generating Station indicated the dependence of the fraction of core exceeding a specified temperature on the power distribution in the active core. The two-dimensional RZ model produced conservative results for this calculation as long as no effort was made to associate region temperature and region age. It was not conservative for use in estimating the maximum fuel temperature; however, this temperature occurred in the higher power regions, which were those regions where the fuel had the lowest exposure.

8.3.2 Thermal Analysis Involving Age Dependence in Fuel

The specified temperatures for the previous analyses were based on values associated with fuel with the highest exposure to irradiation. These values were lower than those for fuel with a shorter exposure to irradiation. Thus, at any time the above analysis yielded greater

percentages of active core whose temperatures exceeded the specified values than would have been calculated if the values were a function of the fuel's age.

It was desired to refine the analysis by calculating the percentages of active core whose temperatures exceeded three specified values where the values were a function of the fuel's age. Thus, the three-dimensional model⁴ for the Fulton Generating Station was revised to simulate as accurately as possible the different power levels and ages of the refueling regions in the active core in that model.

The results from these calculations yielded the percentages of active core whose temperatures exceeded three specified values which were a function of the age of the fuel. Figure 8.4 presents only the totals

Fig. 8.4. Comparison of Percentage of Active Core Above Specified Temperatures for Three-Dimensional Models With and Without Fuel Age Dependence Following LOFC Accident, Fulton Generating Station. Temperatures are 1040, 1130, and 1280°C.

from the results of this analysis; that is, each curve describes the combined volumes of all age groups that exceed the respective criteria describing the same condition of the fuel in all age groups. For comparison, Fig. 8.4 also shows the percentages of core above three specified temperatures, based on fuel with the highest exposure, from a similar analysis independent of the age of the fuel. More detailed results concerning this analysis are presented elsewhere.⁵

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9. DEVELOPMENT AND USE OF THE COUPLED CONDUCTION-CONVECTION MODEL FOR CORE THERMAL ANALYSIS

W. D. Turner

ORNL is charged with providing technical assistance to the Gas-Cooled Reactors Branch (GCRB) of the Directorate of Licensing (DOL) in the safety evaluation of the transient response of active cores of HTGRs under abnormal operating conditions. The coupled conduction-convection model (CCCM) computer code had been developed to provide localized maximum temperatures in the fuel, graphite, and coolant channel of an HTGR during normal operation as well as during accident conditions involving reduced flow and loss of forced circulation. The capabilities of this computer code were extended to allow a thermal analysis of a wider range of accident conditions involving reductions or blockages in the flow of coolant in core segments during normal operation. At the specific request of DOL, we evaluated the thermal effects following certain accident conditions. In particular, the CCCM computer code was used in a thermal analysis involving a single blocked coolant channel in a fuel element. A parametric study analyzed the effects on fuel and coolant temperatures due to variations in the power level, coolant mass velocity, and coolant inlet temperature. We analyzed the effects on the maximum fuel temperature due to a totally closed orifice in the highest powered refueling region. An analysis began of the thermal response of the Fort St. Vrain HTGR following a design-basis depressurization accident with operation of two circulators at 7000 rpm.

9.1 DEVELOPMENT OF THE COUPLED CONDUCTION-CONVECTION MODEL -- W. D. Turner and K. W. Childs

The coupled conduction-convection model (CCCM) computer code is a version of the general heat transfer code HEATING3¹ especially modified for thermal analysis of the HTGR active core during certain abnormal operating conditions.²⁻⁴ Previously, the CCCM computer code was designed to calculate the steady-state and transient

temperature distributions for two-dimensional RZ models coupled to a single one-dimensional axial coolant channel. Three-dimensional $R\theta Z$ models were required for the calculation of temperatures that would develop near a single blocked coolant channel in a fuel element and coolant temperatures that would develop in adjacent unblocked coolant channels. The CCCM computer code was modified to solve these three-dimensional $R\theta Z$ models. The resulting computer code still allows a coolant channel involving fluid flow along the z axis, but now the code allows more than one nodal point on the surface of the channel at each axial position. This modification was necessary to obtain the proper resolution in the θ direction in the solid. These additional nodes are used to transfer heat between the surface of the coolant channel and the coolant itself. However, the calculations in the coolant channel are still restricted to a one-dimensional axial analysis.

9.2 THERMAL ANALYSIS INVOLVING BLOCKED COOLANT CHANNEL AWAY FROM EDGE OF FUEL ELEMENT — W. D. Turner and K. W. Childs

Steady-state and transient calculations were performed for the 2000-MW(t) core to obtain the maximum temperature in the fuel when a single coolant channel located near the center of a fuel element was suddenly completely blocked during normal full-power operation. The model that was used in this analysis is discussed elsewhere.⁵

The ultimate maximum steady-state temperature in the fuel was 1649°C (3000°F), and it occurred just in the upper third of the core near the center of the fuel column. Steady-state temperatures as a function of axial position in the 2000-MW(t) core are shown in Fig. 9.1. About 90% of the change from normal operating temperatures to these temperatures occurred in the first 6 min following a sudden blockage of a coolant channel.

Figure 9.1 presents axial profiles of the fuel centerline temperature (the maximum fuel temperature was not at the center of the fuel under these conditions), the graphite temperatures at

Fig. 9.1. Axial Temperature Profiles During Normal Operating Conditions in the Vicinity of a Blocked Coolant Channel Away From the Edge of the Fuel Element, Summit Power Station. Graphite temperatures are on a line connecting the center of the fuel rod with the center of the blocked coolant channel. Coolant temperatures are those in the adjacent unblocked channel. Temperature ($^{\circ}\text{C}$) = $(5/9)$ [temperature ($^{\circ}\text{F}$) - 32]. Distance in meters is 0.3048 times distance in feet.

the surfaces of the fuel hole and the coolant hole, and the coolant temperature in the adjacent unblocked coolant channel. The graphite temperatures shown are on a line connecting the center of the fuel and the center of the blocked coolant channel. The axial peak graphite temperature at the fuel hole drops off with direction from the 1508.8°C (2747.8°F) shown in Fig. 9.1 to 1188.6°C (2171.5°F) in the direction of an unblocked coolant channel.

The coolant exit temperature from the coolant channel adjacent to the blocked coolant channel increases from 831°C (1528°F) at time of blockage to 910°C (1670°F) in less than 6 min. The transient analysis of temperatures in the vicinity of the blocked coolant channel at the axial position where the highest temperatures are developed is shown in Fig. 9.2. The steady-state values for each of the temperatures analyzed are presented in Table 9.1.

Fig. 9.2. Transient Analysis of Temperatures in the Vicinity of a Blocked Coolant Channel Away From Edge of Fuel Element at an Axial Position 3.237 m (10.62 ft) From Top of Upper Reflector Block, Summit Power Station. Blockage occurs at time equal to zero. Graphite temperatures are on surfaces of fuel and coolant holes on lines from the center of the fuel rod to the center of the blocked or unblocked channels. Temperature ($^{\circ}\text{C}$) = $(5/9)$ [temperature ($^{\circ}\text{F}$) - 32].

Table 9.1. Steady-State Temperatures at Selected Points Involving Blocked Coolant Channel Away From Edge of Fuel Element, Summit Power Station

Position	Temperature	
	($^{\circ}\text{C}$)	($^{\circ}\text{F}$)
Fuel centerline	1631	2968
Graphite at fuel toward blocked	1509	2748
Graphite at fuel toward unblocked	1189	2172
Graphite at coolant, blocked	1487	2708
Graphite at coolant, unblocked	949	1741
Coolant exit	913	1676

If the fuel begins to fail because of thermal stresses at 1500°C (2732°F), a limited volume of the fuel rods adjacent to the blocked channel will have some failed particles over an axial length of about 1.2 m (4 ft, about 1.5 fuel elements) in the high-power region. Most of the transient to the higher temperatures occurred in 6 min following sudden and complete blockage of the coolant channel.

9.3 THERMAL ANALYSIS INVOLVING BLOCKED COOLANT CHANNEL AT THE EDGE OF A FUEL ELEMENT — W. D. Turner and K. W. Childs

Steady-state transient calculations were performed for the 2000-MW(t) core to obtain the maximum temperature in the fuel when a single coolant channel located near the edge of a fuel element was suddenly completely blocked during normal full-power operation. The model and method used for this calculation are described elsewhere.⁵

The results shown in Fig. 9.3 indicate the increase in centerline fuel temperature. A temperature of 2162°C (3924°F) was a conservative estimate of the maximum obtained in this case since all the heat

Fig. 9.3. Axial Temperature Profiles During Normal Operating Conditions in the Vicinity of a Blocked Coolant Channel Near the Edge of a Fuel Element, Summit Power Station. Temperature ($^{\circ}\text{C}$) = $(5/9)$ [temperature ($^{\circ}\text{F}$) - 32]. 1 ft = 0.3048 m.

from the fuel rod at the edge was transferred to an interior coolant channel; all conduction and radiation to the adjacent fuel element was neglected.

9.4 RESULTS OF PARAMETRIC STUDIES IN THE HIGHEST POWERED REFUELING REGION — W. D. Turner and I. I. Siman-Tov

A parametric study was concerned with the development of fuel and coolant temperatures resulting from a normal reactor trip from 100% power for a 2000-MW(t) reactor. Variations within a tolerance of $\pm 10\%$ were imposed on the three following parameters:

1. the radial power peaking factor,
2. the coolant mass velocity,
3. the inlet coolant temperature.

Table 9.2 presents the change in the maximum fuel temperature and the coolant exit temperature as functions of changing parameters at full-power operating conditions. The axial temperature profiles and temperature transients are reported elsewhere.⁵

Table 9.2. Changes in Maximum Fuel Temperature and Coolant Exit Temperature for Variations in Power Density, Coolant Flow and Inlet Temperature From Normal Operating Conditions, Summit Power Station

Parameter	Factor Times Nominal ^a	Maximum Fuel Temperature		Coolant Exit Temperature	
		(°C)	(°F)	(°C)	(°F)
Power density	1.0	1046	1914	837	1538
	1.1	1116	2040	887	1628
	0.9	976	1788	787	1449
Coolant flow	1.0	1046	1914	837	1538
	1.1	845	1853	792	1457
	0.9	1087	1988	892	1638
Inlet temperature	1.0	1046	1914	837	1538
	1.1	1081	1977	872	1602
	0.9	844	1852	801	1474

^a Nominal conditions in a hot refueling region:

Power density: 6.37 MW/m^3 ($6.15 \times 10^6 \text{ Btu hr}^{-1} \text{ ft}^{-3}$)

Flow: $167.86 \text{ kg/m}^2 \text{ sec}$ ($1.2377 \times 10^5 \text{ lb hr}^{-1} \text{ ft}^{-2}$)

Inlet temperature: 339°C (642°F)

The results indicated that the temperature variations in the hottest refueling region due to uncertainties in the values of the inlet temperature, the coolant mass velocity, and the power in the region would fall within the acceptable values and would not approach the allowed damage limits of the core constituents.

The computations in this study were based on a single channel unit cell² using CCCM, and the assumptions incorporated in that model were applicable to these calculations.

9.5 TEMPERATURE DISTRIBUTION IN THE HIGHEST-POWERED REFUELING REGION FOR A TOTALLY CLOSED ORIFICE CONDITION — W. D. Turner and I. I. Siman-Tov

An analysis was performed to evaluate the effects on the maximum fuel temperature in the highest powered refueling region (radial power peaking factor = 1.6) due to a totally closed orifice for that region. When an orifice to a refueling region is totally closed, the opening is not sealed. Thus, some coolant is still supplied to cool the refueling region, but the mass velocity to that region is greatly reduced from that of normal operation. Two cases were run on CCCM to simulate the closed orifice condition. One assumed 15% of the normal flow to the refueling region⁶, while the other case⁷ assumed 25%.

Figures 9.4 and 9.5 present the maximum fuel temperature as a function of time for the two cases. These results were conservative since the model assumed no conductance to the neighboring regions, which were adequately cooled. The temperatures in these two calculations reached 2200°C (4000°F) within 4 to 5 min.

9.6 THERMAL CALCULATIONS INVOLVING A RAPID DEPRESSURIZATION ACCIDENT FOR THE FORT ST. VRAIN HTGR — W. D. Turner and I. I. Siman-Tov

We started a thermal analysis using a single-channel unit cell and the CCCM computer code to evaluate the transient temperatures in the core and coolant for the Fort St. Vrain HTGR following a Design-Basis Depressurization Accident (DBDA) with operation of two circulators at 7000 rpm.

Fig. 9.4. Maximum Fuel Temperature Transient Under Closed Orifice Flow Conditions. Nominal flow is $167.86 \text{ kg/m}^2 \text{ sec}$. Temperature ($^{\circ}\text{C}$) = $(5/9) [\text{temperature } (^{\circ}\text{F}) - 32]$.

Fig. 9.5. Maximum Coolant Channel Wall Temperature Transient Under Closed Orifice Flow Conditions. Nominal flow is $167.86 \text{ kg/m}^2 \text{ sec}$. Temperature ($^{\circ}\text{C}$) = $(5/9) [\text{temperature } (^{\circ}\text{F}) - 32]$.

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10. DEVELOPMENT OF THE MULTICHANNEL CONDUCTION-CONVECTION PROGRAM, HEXEREI

G. E. Giles

The HEXEREI code is a hexagonal and cylindrical mesh conduction heat transfer code with convection to axially flowing coolant. HEXEREI will consist of a model generator merged with modified subroutines from CCCM, new coolant and output routines, flow redistribution orifice sizing routines, and the ACHERONS series of subroutines to give total Core Auxiliary Cooling System (CACS) response.

10.1 HEXEREI CODE — G. E. Giles

The HEXEREI computer code, which has been under development since the latter part of FY 1974, is being designed to analyze the transient response of an HTGR core during emergency cooling situations involving the CACS. More specifically, it will be able to calculate the average temperature profile in each refueling region column, the average temperature profile in the coolant channels in each refueling region, and the maximum and average temperatures of the coolant as it leaves the core.

HPOINT, the mesh generator for HEXEREI, was designed to model a general cylindrical heat conduction problem where the interior portion of the cylinder was best described by a two-dimensional hexagonal mesh in a plane perpendicular to the axis of the cylinder. The hexagonal mesh plane is repeated at each level along the axis where nodes are desired. Based on HPOINT's information, HTHRMP calculates the thermal properties, effective conductance and heat capacity, heat generation, initial temperature for the model, and boundary conditions. This information is built into arrays that merge with the calculating subroutines HCALQT and HTRANO.

Subroutine HCALQT is a modified version of CCCM's CALQLT and performs the steady-state and explicit transient analysis. It calls HTRANO, which is a modified version of CCCM's TRANO. Then HTRANO performs the implicit transient analysis. Both the above routines can handle temperature-dependent material properties, time- and/or temperature-dependent boundary conditions, and heat generation. However, at present only simple boundaries are provided for in HTHRMP.

Subroutines EXPAME and PROPHE build the arrays required to calculate the coolant temperature and heat transferred from the solid nodes to the coolant nodes. At present, each refueling region is modeled by one solid node, which is connected to one coolant node. The coolant node therefore represents all the coolant channels in one refueling region.

EXPAM uses the steady-state method to calculate the coolant temperatures, which neglect the transient heat capacity of the coolant represented by the coolant node.

Since the code will model the performance characteristics of the CACS, it will evaluate the performance of the Core Auxiliary Heat Exchanger (CAHE) and the auxiliary circulator. Thus, it will calculate the transient temperature, mass velocity, and pressure of the coolant as it enters the reactor core. Since the thermal hydraulics in the active core will include calculations of pressure drop due to the orificing, the entrance and exit losses, the friction loss, and the effects due to acceleration and buoyancy, the code will compute the distribution of coolant to each refueling region, as well as the pressure drop across the core.

The code HEXEREI will require as initial conditions certain parameters based on the normal operation of the reactor. When feasible, features will be incorporated into HEXEREI to calculate these parameters to make the code as self-contained as possible.

Although the code is being designed to model each refueling region, additional capabilities are being planned to allow for

detailed studies of portions of the core, such as models of individual fuel elements, portions of fuel elements and portions of the active core.

To facilitate its usefulness, the code is being designed to require a minimum of effort for input data preparation on the part of the user. It will require some description of the basic design of the HTGR in question. From this, the code will generate the nodal description and the related data that are necessary to do the thermal hydraulic analysis.

Code development work has been completed on the generation of the nodal description in the active core as well as the reflector elements and the calculation of effective conductances and other coefficients necessary for the thermal analysis. These algorithms were designed to produce data compatible with the numerical techniques from CCCM, which calculate the steady-state and transient temperature distribution in the core. Current development work involves the meshing of these subroutines containing the developed algorithms with those from HEATING5 that are necessary to calculate the temperature response and do not require major reprogramming.

10.2 FLOW DISTRIBUTION ROUTINES — R. M. DeVault

Development work is also continuing on the calculation of the flow distribution in the active core. This feature will include previous development work by D. D. Paul (Chap. 14).

Subroutine FLODIS will distribute the coolant mass flow into the model's coolant channels on the basis of the latest solid node temperature, coolant inlet temperature pressure, total flow rate, and composition. It uses the orifice areas that are fixed by the reactor full-power steady state. To simplify use of the code, subroutine ORFICE calculates the orifice settings for a full-power steady state and stores or outputs them so that they can be used for an accident transient investigation. It adjusts

the orifice settings to drive the coolant exit temperatures to an average exit temperature for all channels. The steady-state technique is then reentered to produce a new solution. Subroutine ORFICE then repeats these two steps until all exit temperatures are equal.

Both HEXEREI and the flow distribution routines are in the debug stage, but we expect soon to be able to do calculations under the limitations outlined by Turner.¹ The ACHERONS program will be merged into HEXEREI in early FY 1976, and the flow distribution calculations will be expanded to include reverse flow. Other refinements will be added as outlined.¹

10.3 REFERENCE

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11. COOLING SYSTEM PERFORMANCE AFTER SHUTDOWN

J. R. Tallackson

All work during the period and discussed in this section was related to evaluating performance of core auxiliary heat exchangers (CAHEs) in HTGRs. Two operational modes were considered: (1) normal operation, in which pure pressurized helium is the core coolant and (2) operation under conditions following the design-basis depressurization accident (DBDA) and a loss of main loop coolant (LOMLC) when the core coolant is a time-varying mixture of helium diluted with air and the carbon monoxide produced by air reacting with hot graphite. This activity may be subdivided into three categories:

1. computer code development,
2. establishing the thermal transport properties of the materials involved in CAHE heat transfer,
3. producing and reporting heat transfer performance computations for conceptually designed CAHEs in the Summit Power Station and Fulton Generating Station HTGRs.

The material in this chapter is reported in more detail in other documents.¹⁻⁵

11.1 COMPUTER PROGRAMS FOR CALCULATING CAHE PERFORMANCE

Program development³ during this period consisted of upgrading and expanding the original CACHE series of programs designed to compute heat transfer rates with pure helium on the gas side. Similar programs were written for use when the core coolant gas is a mixture of varying composition existing after a DBDA + LOMLC. Initially all the programs required that the user specify boundary temperatures at the cold end of the CAHE. A further modification enabled the user to specify the hot gas and cold water temperatures as boundary conditions.

Evolution continued with the result that the several versions of the code were consolidated into a single program,⁶ renamed simply CACHE. This program enables the user to compute CAHE performance for cases when the core coolant is pure helium or a post-accident (DBDA + LOMLC) gas mixture; it also enables the user to specify either the cold-end CAHE temperatures or the gas and water inlet temperatures (hot gas and cold water).

The dimensions and flow rates in the Summit and Fulton Station CAHEs were established by two programs, CONFIG and FULTN1, respectively. These were linked to the CACHE programs with a "chain" command.

11.2 TRANSPORT PROPERTIES FOR CACHE

Calculation of heat removal rates with the CACHE programs required equations for the thermal transport properties of the materials (gases, water, and steel) through which heat is transferred in a CAHE. The principal effort centered on satisfactory methods of evaluating the thermal conductivity and viscosity of the gas mixtures after a DBDA + LOMLC. The atmosphere in the PCRV then consists of a mixture of He, N₂, O₂, and CO. The composition varies with the elapsed time after the accident; the variation, expressed as molecular weight of the mixture, was determined to be in the range of 4.00 to 20.0.

A literature search indicated that theoretical calculations of conductivity and viscosity of mixtures of this type are algebraically complex and not extremely accurate. Therefore, the experimental data reported by Touloukian et al.,⁷ and Johnson⁸ were used to develop the equations for gas mixture thermal conductivity and viscosity in the CACHE programs. In using these data, we assumed that helium-nitrogen, helium-oxygen, and helium-air mixtures and combinations thereof behave similarly. Mixture heat capacity presented no problem; the weight fraction averages of helium and air specific heats were used.

We reported¹ that the first equation for gas mixture conductivity gave lower than correct values when the molecular weight of the mixture was low. The lowered heat transfer rates resulting from the use of this equation in the CACHE⁴ program were not seriously in error, and the error became negligible for conditions 1.3 hr after the DBDA, when the molecular weight of the mixture was greater than 11.0.

During the fourth quarter of 1974 the mixture conductivity equation was revised,⁴ and the difference between the measured and calculated mixture conductivities at the temperatures of interest [$\sim 300^{\circ}\text{C}$ (600°F)] became negligible. The equation for mixture viscosity was also revised slightly improving accuracy.

Figures 11.1 and 11.2 show curves of gas mixture conductivity and viscosity currently in use. All thermal transport properties developed to calculate CAHE heat transfer rates are being reported.⁹

Fig. 11.1. The Thermal Conductivity of Helium-Nitrogen Gas Mixtures from Equations in the CACHE Program for Calculating CAHE Heat Transfer Rates and the Thermal Conductivities of Pure Helium and Pure Nitrogen.

Fig. 11.2. The Viscosity of Post-DBDA Gas Mixtures From the Equation in the CACHE program for Calculating CAHE Heat Transfer Rates.

11.3 CAHE HEAT TRANSFER PERFORMANCE CALCULATIONS

During normal postshutdown operation with pure pressurized helium on the gas side, Table 6.3.2-1 in the Summit Station PSAR¹⁰ specified, as of the first quarter in 1974, a CAHE duty of 68.2 MW (2.33×10^8 Btu/hr). ORNL calculations¹ verified this heat transfer capability. Similar computations by ORNL for conditions assumed to exist after a DBDA + LOMLC were at variance (~20% low) with tabulated performance data¹⁰ provided by the designers. The performance tabulations were not entirely complete, and no final conclusions were possible. The lower performance rates calculated by ORNL were thought to be caused by differences in the methods used to calculate gas thermal conductivity. The gas thermal conductivity evaluation error noted in Sect. 11.2 was not considered responsible for the difference since the 20% performance difference included

performance computations at high gas mixture molecular weights (>11.0). These and other calculations were reported to the regulatory agency of NRC.

During the second quarter in 1974, a study² indicated that errors of 20% or less in evaluating gas thermal conductivity are reflected by equal errors in the overall heat transfer rate by a CAHE.

During the third quarter of 1974 calculations to determine Fulton Station CAHE performance were produced.³ We concluded that with normal postshutdown operating conditions the specified performance, 76 MW (2.6×10^8 Btu/hr), would be met, providing the helium inlet temperature to the CAHE was not less than 860°C (1580°F). This conclusion contained the assumption that 90% of the tubes are effective. The results of these calculations based on 100% effective area are on Fig. 11.3. The post-DBDA CAHE performance was also computed, but subsequent modifications in system design made these calculations obsolete.

In response to Amendment 26 (draft)¹⁰ a revised set of post-DBDA calculations for the Summit Station CAHE was provided. The results of these revised heat transfer calculations are presented in Fig. 11.4. They showed that the specified duty, 29.3 MW (1.00×10^8 Btu/hr), will be met if the inlet gas temperature is in the range 1016 to 1060°C (1860–1940°F), depending on whether the "effective area" is taken to be 100% or 90% of the total tube surface. It was necessary to assume that these computations were applicable for a gas mixture molecular weight of 10.3 at 1.1 hr after initiation of the accident.

Station CAHE performance computations were produced in an effort to keep abreast of a continually changing set of post-DBDA operating parameters. As reported,⁴ these calculations were unreliable because of inconsistencies and errors in the specified operating conditions received piecemeal by ORNL as ad hoc, informal

Fig. 11.3. Calculated Heat Transfer by the Core Auxiliary Heat Exchanger with Pressurized Helium in the 3000-MW(t) Fulton HTGR. Basis: helium flow rate, 28.5 kg/sec (226,000 lb/hr); H₂O flow rate, 144 kg/sec (1.14×10^6 lb/hr); H₂O inlet temperature, 80.3°C (176.5°F). Temperature (°C) = (5/9)[temperature (°F) - 32]. 1 Btu/hr > 0.293 W.

Fig. 11.4. Calculated Performance of a Core Auxiliary Heat Exchanger (CAHE) in the Summit Station HTGR after a DBDA with cooling by 1 CACS loop. Temperature (°C) = (5/9)[temperature (°F) - 32]. 1 Btu/hr = 0.293 W.

transmittals. This situation was corrected in early 1975, and the performance curves of Fig. 11.5 were computed. It was concluded that the conceptual design of the Fulton Station CAHE was adequate.

Fig. 11.5. Expected Performance of Two Fulton Generating Station CAHEs as Calculated by GA and ORNL. Cooldown after a DBDA + LOMLC at 105% power [3150 MW(t)]; containment backpressure = 24.4 psia = 168 Kpa. 1 Btu/hr = 0.293W.

11.4 REFERENCES

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3. Ibid., *HTGR Safety Studies Quart. Prog. Rep.* Sept. 30, 1974, for the Gas-Cooled Reactors Branch, Directorate of Licensing, ORNL/TM-4806, pp. 24-39.
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12. EVALUATION OF CIRCULATOR AND MOTOR FOR THE CACS

R. L. Reid

The objective of this study was to analyze the circulator and motor capabilities for the core auxiliary cooling system (CACS) during a design-basis depressurization accident (DBDA) plus loss of main loop cooling (LOMLC) from tabulated estimates by GA of coolant flow rates and molecular weights as functions of time. The input information sources, calculations, and results are reported elsewhere.¹

Calculating the pressure losses through the core and heat exchangers and estimating the outlet and inlet losses give a total of 3.8 kPa (0.55 psi) immediately on depressurization and 5.5 to 6.7 kPa (0.8–1.0 psi) after air inleakage. These agree well with the performance line, which relates pressure loss to flow rate. From the pressure losses and compressor characteristics were calculated the required motor power, speed, and torque. All combinations of torque and speed were within the performance limits of the motor. Thus, from the assumptions given, the compressor and motor specified by GA are adequately designed to provide the desired coolant flow rates during a DBDA plus LOMLC.

The torque requirements of the CACS motor were reviewed² following a calculation of the air ingress rate after a DBDA (see Chap. 15 of this report). The computer program predicts higher molecular weights after a DBDA than those used in the circulator and motor evaluation. Since the circulator speed and motor torque requirements depend on molecular weight, we reexamined² these requirements. The recalculated values are compared with the previous values in Table 12.1. The two larger torque values exceed the limits given by GA.

Actually, the power will probably also increase since the head loss will be higher, and a higher mass flow rate will be required because of the poorer heat removal ability of the

Table 12.1. Reevaluation of Circulator and Motor Requirements

Time After DBDA (hr)	Circulator Speed, rpm		Torque Requirements, N-m (lbf-ft)	
	GA Assumptions	Computer Predictions	GA Assumptions	Computer Predictions
0.0833	3486	3334	1337 (986)	1375 (1014)
1.0064	2922	2563	1724 (1271)	1965 (1449)
2.1964	2627	2288	1618 (1193)	1859 (1372)

higher molecular weight gas. Therefore, it appears that the motor must develop at least 2 kN-m (1500 lbf-ft) at about 2500 rpm.

12.1 REFERENCES

1. R. L. Reid, "Evaluation of Circulator and Motor for the CACS," *HTGR Safety Studies Quart. Prog. Rep. June 30, 1974 for the Gas-Cooled Reactors Branch, Directorate of Licensing, Office of Regulation, USAEC, ORNL/TM-4737*, pp. 39-45.
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13. DEVELOPMENT OF THE CORE AUXILIARY COOLING SYSTEM CALCULATION, ACHERONS

G. E. Giles

The subroutines detailed below were developed to provide an independent analysis capability of the Core Auxiliary Cooling System (CACS) and to be added to the HEXEREI computer code (Chap. 10). The subroutine HELIX was developed from work done by J. R. Tallackson (Chap. 11) and the subroutine AEOLUS was based on work done by R. L. Reid (Chap. 12). Several routines were designed to make the program as simple to use as possible and to merge smoothly with the HEXEREI code. Results of calculations for the Fort St. Vrain and Fulton reactors are also presented.

Editor's Note: This chapter reports an extensive series of calculations done before adoption of SI units by ORNL. The extensive use of English units in empirical equations and graphically presented results makes conversion to SI impractical. The following list is provided to enable the reader to convert the values given in the conditions and results.

Area: $1 \text{ ft}^2 = 0.09290 \text{ m}^2$

Conductivity: $1 \text{ Btu/hr-ft-}^\circ\text{F} = 1.730 \text{ W/m K}$

Density: $1 \text{ lb/ft}^3 = 16.02 \text{ kg/m}^3$

Flow rate: $1 \text{ lb/sec} = 0.4536 \text{ kg/sec}$

Length: $1 \text{ in.} = 25.4 \text{ mm}$

Power: $1 \text{ Btu/hr} = 0.2929 \text{ W}$

Pressure: $1 \text{ lbf/ft}^2 = 47.88 \text{ Pa}$

$1 \text{ psi} = 6895 \text{ Pa}$

Specific heat: $1 \text{ Btu/lb-}^\circ\text{F} = 4187 \text{ J/kg K}$

Temperature: $1^\circ\text{R} = (5/9) \text{ K}$

$\text{Temperature } (^\circ\text{C}) = (5/9)[\text{Temperature } (^\circ\text{F}) - 32]$

Torque: $1 \text{ lbf-ft} = 1.356 \text{ N m}$

Viscosity: $1 \text{ lb/hr-ft} = 0.0004134 \text{ Pa sec}$

13.1 SUBROUTINE HELIX — K. W. Childs, R. M. DeVault, and G. E. Giles

Much of the developmental work commenced in the second quarter of 1974. The first step was to develop a group of subroutines to analyze the performance of the Core Auxiliary Heat Exchangers (CAHE) in HTGRs. Since the geometry of the heat exchanger is constant along its axis, it can be modeled, for the purpose of calculations, as a simple concentric shell and tube arrangement with the appropriate heat transfer coefficient applied to each surface of the tube. This model is shown in Fig. 13.1. The heat transfer correlations and thermal properties used in the evaluation of this model are presented in the following paragraphs.

Fig. 13.1. Model for Analysis of Core Auxiliary Heat Exchanger in Subroutine HELIX of ACHERONS.

The convective heat transfer coefficient on the water side of the tube is determined by the Dittus-Boelter correlation:

$$Nu_i = 0.023 Re_i^{0.8} Pr^{0.4}, \quad (1)$$

where

$Nu_i = h_i D_i / k$ = Nusselt number (dimensionless),

$Re_i = D_i G / \mu$ = Reynolds number (dimensionless),

$Pr = C_p \mu / k$ = Prandtl number (dimensionless),

D_i = inside diameter of tube,

G = mass flux of water,

k = conductivity,

μ = viscosity,

C_p = heat capacity.

The properties of water are evaluated at the bulk water temperature.

The convective heat transfer coefficient on the gas (helium or helium-air mixture) side of the tubes is determined by a modified Grimson correlation assuming that the tube bundle is a combination of both in-line and staggered tube arrangements.¹

$$Nu_o = X_1 F_1 C_1 Re_o^m Pr^{0.33} + X_2 F_2 C_2 Re_o^n Pr^{0.33}, \quad (2)$$

where

X_1, X_2 = fraction of tubes that are staggered and in line, respectively,

C_1, C_2 = leading constants for staggered and in-line tubes, respectively, which are functions of lateral and longitudinal spacing,

m, n = exponents for staggered and in-line tubes, respectively, which are functions of lateral and longitudinal tube spacing,

F_1, F_2 = "arrangement factors," functions of lateral and longitudinal tube spacing and Reynolds number,

$$Re_o = D_o G / \mu = \text{Reynolds number (dimensionless)},$$

$$Nu_o = h_o D_o / k = \text{Nusselt number (dimensionless)}.$$

The properties on the gas side are evaluated at the gas-side film temperature. In the computer program, the coefficients F and C are combined into a single factor.

The overall heat transfer coefficient, U , based on the outside area is

$$1/U = 1/h_o + LD_o/k_w D_{lm} + D_o/h_i D_i,$$

where

h_o = outside heat transfer coefficient,

h_i = inside heat transfer coefficient,

D_o = outside diameter of tube,

D_i = inside diameter of tube,

L = tube wall thickness,

$$D_{lm} = (D_o - D_i) / \ln(D_o / D_i),$$

k_w = conductivity of tube wall.

The properties of a mixture of helium and air are determined as shown below.

Viscosity*

$$\mu_{\text{He}} = 5.942 \times 10^{-4} T_a^{0.71}, \quad (3)$$

* Equations obtained by curve fitting tables of Keenan and Kaye.²

where

μ = viscosity ($\text{lb}_m/\text{hr-ft}$),

T_a = absolute temperature ($^{\circ}\text{R}$).

Conductivity*

$$\begin{aligned} K_{\text{mix}} = & 0.0399/(0.27716 + X_1) - 0.3196 \times 10^{-2} \\ & + 0.07014X_1 - 0.15767X_1^2 \\ & + 0.08618X_1^3, \end{aligned} \quad (4)$$

where

$$X_1 = \text{AMW} - \text{AMW}_2/(\text{AMW}_1 - \text{AMW}_2),$$

and

AMW = molecular weight of mixture of air and helium,

AMW₁ = molecular weight of air,

AMW₂ = molecular weight of helium,

k = conductivity ($\text{Btu/hr-ft-}^{\circ}\text{F}$),

T_a = absolute temperature ($^{\circ}\text{R}$).

Specific Heat

$$\begin{aligned} C_{\text{air}} = & 0.2438 - 0.29401 \times 10^{-4}T_a \\ & + 0.45436 \times 10^{-7}T_a^2 - 0.11210 \times 10^{-10}T_a^3, \end{aligned} \quad (5)$$

$$C_{\text{He}} = 1.2425, \text{ (ref. 3)} \quad (6)$$

$$C_{\text{mix}} = y_{\text{air}}C_{\text{air}} + y_{\text{He}}C_{\text{He}}, \text{ (ref. 4)} \quad (7)$$

*Equations obtained by curve fitting tables of Keenan and Kaye.²

where

C = heat capacity (Btu/lbm-°F),

T_a = absolute temperature (°R),

y = mass fraction of air or helium.

The temperature-dependent properties of water used in the calculations are determined from the following expressions:

Viscosity*

$$\mu = 378.4/T^{1.81} , \quad (8)$$

Conductivity*

$$k = 0.31 + 6.91 \times 10^{-4}T - 1.48 \times 10^{-8}T^2 , \quad (9)$$

Specific Heat

$$C = 1.035 - 4.55 \times 10^{-4}T + 1.38 \times 10^{-6}T^2 , \quad (10)$$

where

μ = viscosity (lbm/hr-ft),

k = conductivity (Btu/hr-ft-°F),

C = heat capacity (Btu/lbm-°F),

T = temperature (°F).

Subroutine HELIX does the actual performance calculations on the heat exchanger. HELIX is called from a main program, which supplies geometric information, flow rates, and inlet temperatures necessary

*Equations obtained by curve fitting tables of Keenan and Kaye.²

for the calculations. In the course of its computations, HELIX calls several other subroutines either directly or indirectly. These subroutines along with their purposes are listed below.

DITTUS — Calculates the convective heat transfer coefficient inside tubes using the Dittus-Boelter correlation.

GRIMSN — Calculates the convective heat transfer coefficient outside the tubes using a modified Grimson correlation.

PRPH20 — Calculates the temperature-dependent properties of water.

PRPHE — Calculates the temperature-dependent properties of helium-air mixtures.

PRPW — Calculates the temperature-dependent properties of the tube wall material.

13.2 SUBROUTINE TUBE — R. M. DeVault

Subroutine TUBE calculates the coefficients C and N , which are used in obtaining the Nusselt number, Nu :

$$Nu = CRe^n, \quad (11)$$

$$C = C_1 C_G / Pr^{1/3} \quad (12)$$

where

C_1 = coefficient as in Eq. (2),

$Pr = 0.69$ = Prandtl number of air required so that gases with different Prandtl numbers can be evaluated.

C_G = Adjustment of Grimson correlation due to number of rows considered.

Re = Reynolds number.

Subroutine TUBE obtains the Nusselt number coefficient from a method developed by J. P. Sanders using the best fit to the statistical trend for the Grimson coefficients.⁵ This fit was evaluated by

D. F. Hoppes at ORNL against recent proprietary data from the European community; the results were presented in an internal ORNL report titled "Evaluation of Recent Data for Gas-Side Heat Transfer Coefficients for Tube Bundles." That report tabulated the equations and constants needed to calculate the Nusselt number coefficients. Subroutine TUBE stores these constants and uses the relationships from the report to obtain the Nusselt number coefficients. If the transverse or longitudinal distances are not specified exactly in the tables, the subroutine uses an interpolation procedure to obtain those constants necessary for determining the Nusselt number coefficients.

13.3 SUBROUTINE DELTPR — R. M. DeVault

Subroutine DELTPR computes the pressure drop across the CAHE by determining an appropriate friction factor.

The empirical friction factor, f' , was calculated by use of Jakob's approach⁵ for Reynolds number exceeding 1000:

$$f'_1 = \left[0.25 + \frac{0.118}{\left(\frac{S_T - D_o}{D_o} \right)^{1.08}} \right] \left(\frac{G_{MAX} D_o}{N_b} \right)^{-0.16} \quad (13)$$

for staggered tubes, and

$$f'_2 = \left[0.044 + \frac{0.08 S_L / D_o}{\left(\frac{S_T - D_o}{D_o} \right)^{0.43 + 1.13 D_o / S_L}} \right] \left(\frac{G_{MAX} D_o}{N_b} \right)^{-0.15} \quad (14)$$

for in-line tubes. Since the tubes are arranged 75% in line and 25% staggered, the friction factor was assumed to be

$$f = 0.75 f'_2 + 0.25 f'_1, \quad (15)$$

and the pressure drop in lb/ft^2 was calculated by

$$\Delta P = \frac{f G_{\text{MAX}}^2 N}{2.09 \times 10^8} \left(\frac{\mu_s}{\mu_b} \right)^{0.14}$$

where

G_{MAX} = mass velocity at minimum free flow area,

ρ = density (lbm/ft^3),

N = number of transverse rows,

μ_s = viscosity at tube surface temperature,

μ_b = viscosity at helium bulk temperature.

Since the heat exchanger is divided into 12 sections, the change in pressure is calculated for each section from the average properties for that section. The total pressure drop is then obtained by adding the sectional pressure drops.

13.4 SUBROUTINE AEOLUS

The HELIX subroutine described above was merged with subroutine AEOLUS, which calculates the performance of the CACS circulator. The subroutine HELIX supplies the heat exchanger exit gas temperature as the inlet temperature of the circulator. The pressure, temperature, flow rate and molecular weight of the gas are also inputs to AEOLUS, which calculates the system pressure drop from the system line [Eq. (16)] and the power required from the polytrophic efficiency [Eq. (17)].

$$\frac{\Psi}{\Psi_0} = \left(\frac{\phi}{\phi_0} \right)^2, \quad (16)$$

$$\begin{aligned}
 \dot{W}_{\text{shaft}} &= \frac{1}{\eta} \left\{ \frac{RT}{(k-1)/k} \left[\left(\frac{p_e}{p_i} \right)^{(k-1)/k} - 1 \right] \right\} \\
 &= \frac{1}{\eta} \left\{ \frac{RT}{(k-1)/k} \left[\left(\frac{\Delta P}{p_i} + 1 \right)^{(k-1)/k} - 1 \right] \right\}, \quad (17)
 \end{aligned}$$

where

$$\Psi = (\Delta P/P),$$

$$\phi = \left(\frac{\dot{W} \sqrt{T/T_o}}{P/P_o} \sqrt{\frac{R}{R_o}} \right),$$

$$\eta = 0.75,$$

$$\Delta P = p_e - p_i,$$

$$p_e = \text{exit pressure},$$

$$p_i = \text{inlet pressure},$$

$$P_o = 23.2 \text{ psia},$$

$$R_o = 386,$$

$$\dot{W}_o = 23.33 \text{ lb/sec},$$

$$T_o = 880^\circ\text{R},$$

$$\Delta P_o = 0.55 \text{ psi}.$$

Design conditions are noted by subscript o .

These calculations are essentially those presented by Reid⁶ except that he uses the values of ΔP calculated rather than the expression in Eq. (16) and a simpler expression for the shaft work. The close agreement between the system line and Reid's values establishes the validity of using Eq. (16). Since Eq. (17) produces slightly lower values of shaft work than the previous values but is more general, it was utilized.

The AEOLUS subroutine was used to generate the data points in Figs. 13.2 and 13.3 by assuming that the inlet helium properties are

Fig. 13.2. Calculated Results from ACHERONS Program for Summit 2000-MW(t) Reactor During DBDA Plus LOMLC Accident.

Fig. 13.3. Values Calculated by the AEOLUS subroutine for CACS Circulator Performance for Summit 2000-MW(t) Reactor during DBDA Plus LOMLC Accident.

as described⁷ by GA. The results of the AEOLUS subroutines were compared with those of Reid⁶ and agreed well for the first two calculated points.

13.5 INVESTIGATION OF DBDA FOR FULTON REACTOR

The basic ACHERONS computer program was then modified to enable calculation of the Fulton plant Design Base Depressurization Accident (DBDA) and Loss of Main Loop Cooling (LOMLC) accident.

The subroutine that calculates the tube wall conductivity was changed to represent the two materials of the tubes in the CAHE as reported⁸ by GA. The lower 25% portion (hot end) of each tube is now to be made up of 2 1/4 Cr-1 Mo (T22), and the remainder will be made up of the original material, 1/2 Cr-1 Mo (T2).

The circulator performance calculations used the design values of $W_o = 21.1$ lb/sec, $P_o = 22.53$ psia, $\Delta P_o = 0.67$ psi (Fig. 6.3.2-6, ref. 9).

Figure 13.4 shows the CACS performance comparison for DBDA in tabulated results provided⁹ by GA. Figure 13.4(d) through (f) shows the circulator performance comparison for the conservative case DBDA (Table Q6.3-17-3, ref. 9). The circulator torque and speed were not reported in the PSAR⁹ but had been transmitted separately.⁸ Figure 13.5 shows the CACS performance comparison with GA's results⁸ for the DBDA. The major difference stated between these cases is the containment pressure; it was 24.4 psia for the case in the PSAR⁹ and 9.5 psia for the transmittal information.⁸ Figure 13.5(a) through (c) shows the same relative agreement between GA's results and that calculated by the HELIX program. Figure 13.5(d) shows the circulator speed comparison; the AEOLUS subroutine calculates a horizontal line somewhat higher in speed than presented by GA. This difference is due to the higher CAHE exit helium temperature calculated by HELIX. When the temperature presented by GA is used as input data for the AEOLUS subroutine, the resultant circulator speed equals that given⁸ by GA. Since the circulator cannot be operated faster than 3550 rpm, the AEOLUS results indicate that if the helium temperature does increase, then the helium flow rate must decrease.

Fig. 13.4. ACHERONS Results for Fulton (3000 MW) During DBDA and LOMLC Accident (Containment Pressure = 24.4 psi).

Fig. 13.5. ACHERONS Results for Fulton (3000 MW) During DBDA and LOMLC Accident (Containment Pressure = 9.5 psi).

Using the same calculation method that produced good agreement in Fig. 13.5(d) with the PSAR data, Fig. 13.4(d) shows that the circulator speed should be decreasing all during the transient. The solid line is an assumed value of 3550 rpm based on the previous data. Since the AEOLUS results were produced by using the system line with the same design values as in Fig. 13.5(d), it is an indication that the actual torque does exceed the torque limit on the motor and the speed is decreased.

The heat duty [Fig. 13.4(b)] and helium exit temperature [Fig. 13.4(a)] compare very well with GA's results. Much of the difference is attributable to the possibility of using a slightly different Nusselt number correlation for the new heat exchanger configuration. The pressure drop calculated by DELTPR compares very well with those presented in the PSAR, except for the early points. This discrepancy is possibly due to the pseudo-transient method of solution, which gives higher heat transfer rate and gas viscosities due to the lower exit temperature, since the method assumes that the heat exchanger is at pseudo-steady state.

13.6 FORT ST. VRAIN REACTOR COOLING PERFORMANCE INVESTIGATION

During the period February to April 1975, a version of ACHERONS was developed and used to analyze the cooling performance of the steam generators on the Fort St. Vrain reactor during DBDA accident number two. ACHERONS results were compared with those presented by the Public Service Commission of Colorado.¹⁰

13.6.1 Circulator Performance

The circulator performance was taken as depicted in Fig. 3-1, p. 24 of the circulator operation and maintenance manual.¹¹ Figure 13.6 is a copy of this figure with the apparent system line.

By using the temperatures and gas flow rates in the response and assuming the circulator turns at 7000 rpm, the corrected flow rates and mass flow rates are calculated. The compressor map given by GA can

Fig. 13.6. Fort St. Vrain Circulator Performance as in Fig.3-1 of the Plant Operation and Maintenance Manual with Apparent System Line as Generated by AEOLUS Analysis of GA's Results.

be reduced to a single compressor speed curve by use of similarity parameters if the circulator is assumed to operate as an ideal compressor (i.e., no viscous losses, etc.). Similarity parameters:

$$\phi/\phi_0 n = \text{constant} , \quad (18)$$

$$\psi/\psi_0 n^2 = \text{constant} , \quad (19)$$

where

n = circulator speed rpm,

ΔP = pressure rise across circulator,

ϕ/ϕ_0 = corrected mass flow rate/design mass flow rate,

$\psi/\psi_0 = (\Delta P/P)/(\Delta P/P)_{\text{design}}$.

The pressure drop for a particular corrected speed and mass flow rate can be calculated from this information if the pressure drops and flow rates for a constant corrected speed line are known. To check out this technique, the 9550 speed curve was extrapolated to the other speeds and checked against those curves in the compressor map. The agreement was very close and, therefore, the similarity approach could be used with confidence if the compressor map shown was derived from experimental data.

By use of the similarity technique and the data in the response,¹⁰ the pressure drop can be calculated and plotted on the compressor map. This was the manner the apparent system line was deduced.

The reason for using the similarity technique was to force the circulator performance calculations to maintain a speed of 7000 rpm without knowing beforehand what the system line was for the particular case analyzed. The pressure load of the circulator calculated in this manner compares favorably with the loop pressure drops presented in the response and show that the circulator (if it were ideal) would deliver substantially more pressure head than required (Fig. 13.7). Analysis of the viscous losses was not possible within the time limit. The maximum torque required during this transient was 51 lbf-ft.

Fig. 13.7. Fort St. Vrain Circulator Pressure Rise as Calculated by AEOLUS or DBDA Compared with GA Values.

13.6.2 Steam Generator Model Description

Upon leaving the reactor core, the primary coolant of the Fort St. Vrain HTGR flows first through a reheater, then into superheater 2, and finally into the steam generator. The steam generator consists of three sections. The sections, as they are encountered by the primary coolant, are: (1) superheater 1, (2) the evaporator, and (3) the economizer. The flow is countercurrent in all the heat exchangers except superheater 2, which has parallel flow.

Developing a model of the heat transfer system required provisions for the varying physical characteristics of each heat exchanger. This was best done by developing a model that divided the steam generator into three distinct sections. It neglected the reheater since the secondary flow through the reheater is negligible after the depressurization transient has decayed.

The Tube Program calculates the coefficients C and n that are used in obtaining the Nusselt number Nu :

$$Nu = C_1 C_G Re^n, \quad (20)$$

where

C_G = adjustment to Grimson correlation due to the number of rows considered,

Re = Reynolds number.

The model incorporated the physical characteristics from the manual,¹¹ repeated in Table 13.1. The Nusselt number calculated for the steam generator was:

$$Nu = 0.5702 Re^{0.5633} Pr^{1/3}. \quad (21)$$

The Nusselt number correlation calculated for superheater 2 was:

$$Nu = 0.5578 Re^{0.5645} Pr^{1/3}. \quad (22)$$

Table 13.1. Steam Generator and Reheater Physical Characteristics

Characteristic	Single Module Data		
	EES ^a	SH2	RH
Effective heat transfer area without leads, ft ²			
Installed	2480	850	440
Ideal	2120	704	388
Superficial gas flow area, ft ² . Area occupied by:	18.634	15.569	18.201
Tubing	11.657	9.367	11.636
Support structure	0.435	0.420	0.442
Effective tube spacing (pitch), in.	1.42	1.48	1.575
Longitudinal	1.47	1.44	1.575
Tube outside diameter, in.	1.00	1.00	1.125
Tube wall thickness, in.	0.138, 0.138, 0.225	0.205	0.140
Tube material	SA-213 T2, T22, T22	SB-163-GR2 Ni-Fe-Cr	SB-163-GR2 Ni-Fe-Cr
Effective tube length, ft			
Installed	175	60	17.8
Ideal	149.99 ^b	48.81	15.7
Cross-sectional free flow area, ft ²	6.542	5.782	6.123

^a Economizer, evaporators, superheater 1. Entries are given in this order if not the same for all three.

^b Ideal Economizer Length = 76.81 ft
 Ideal Evaporator 1 Length = 25.51 ft
 Ideal Evaporator 2 Length = 23.38 ft
 Ideal Superheater 1 Length = 24.29 ft

TOTAL 149.99 ft

Division of EES *installed* surface not readily available. Recommend using same percentage division as for ideal if needed for analysis.

Each of the four exchanger sections was divided into five subsections to improve both accuracy and convergence. The iterative procedure involved was to guess at the outlet water temperature at superheater 1 and back-calculate the inlet water temperature to the economizer. The heat exchanger calculations were assumed to converge when the calculated value was within a sufficiently small tolerance of the known value.

13.6.3 Investigation Results

As shown in Fig. 13.8, the computed heat duty and helium exit temperature compare favorably with those reported in the response.¹⁰ These results are essentially independent of the helium heat transfer coefficient since the helium flow rate is so small that the helium reaches the feed water temperature well before the end of the heat exchanger is reached. The response indicated that the helium temperature at the core inlet was lower than the feed water temperature because the gas flows over the PCRV thermal barrier, which is cooled by 140°F water. This effect was not analyzed since the helium temperature change is much greater in the heat exchanger. The apparent system line shown on Fig. 13.6 lies very close to the stall line. General Atomic feels that this is due to the omission of bypass flows around the circulators and the active core. The analysis was not redone because of lack of time and information.

The only area of serious disagreement is during the first 0.1 hr of the transient, where our heat duty is much greater than GA's. This is apparently because the HELIX program uses a pseudo-transient technique of driving the steady state solution with transient parameters (i.e., flow and temperature). If the steam generator is slow in responding to changes, then the heat duty will be less than our program indicates. Only a small portion of time and only a 4.4×10^5 Btu difference in total heat transferred (~0.5% of total transferred) is involved. This difference is considered insignificant.

Fig. 13.8. Fort St. Vrain Steam Generator Performance as Calculated by HELIX Compared with GA Calculations. (a) Heat removal rate. (b) Helium exit temperature. (c) Water exit temperature.

13.7 REFERENCES

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4. J. H. Perry, *Chemical Engineers' Handbook*, 4th ed., McGraw-Hill Book Co., New York, 1963, Sect. 3.
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7. D. S. Duncan, Letter to R. A. Clark, Chief, Gas Cooled Reactor Branch, DOL-USAEC, Subject: Answers to Request for Additional Information No. 1 on Licensing Topical Report No. 1 (GA-LTR-1), table on p. Q17-25.
8. Data transmittal from General Atomic Company to J. P. Sanders, dated January 30, 1975.
9. Philadelphia Electric Co., *Fulton Generating Station, Preliminary Safety Analysis Report* DOCKET-50-463.
10. Letter dated February 28, 1975 from R. F. Walker to R. A. Clark containing responses to question 14.11-4.
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14. DEVELOPMENT OF THE FLODIS CODE

D. D. Paul

A lumped parameter model, FLODIS, of the Fort St. Vrain reactor core was developed. This analysis was done to supplement the study of emergency core cooling at reduced flow rates produced by the modification of the Pelton wheel operating specifications. The code calculates the coolant flow redistribution following a design-basis depressurization accident and was developed to supply the CCCM code with region flow rates as a function of time. A preliminary version of the code is operational.

Development of the FLODIS code required capabilities for calculating core temperatures, fluid temperatures, and the flow distribution. These parameters are not independent of each other. An iteration scheme was devised where each one of these parameters could be calculated by assuming the other two were known. The iteration scheme converged when the assumed value for each parameter was within some small convergence criterion of the calculated value.

Since the distribution of the total flow was of primary interest, a detailed temperature distribution in the core was not necessary. Thus, a simplified core model was chosen with only one node per refueling region.

Figure 14.1 shows preliminary results of the single node per refueling region model. Normalized flow is plotted versus time for the hottest channel in the Fort St. Vrain reactor. The normalized flow is defined as

$$W_{\text{norm}} = W_i / W_{\text{av}} ,$$

where

W_i = flow rate in region i ,

W_{av} = average flow

= $\frac{\text{total flow times the number of fuel columns in region } i}{\text{total number of fuel columns}}$

Fig. 14.1. Normalized Flow Calculated by the FLODIS Code for the Hottest Refueling Region in the Fort St. Vrain Core Following the DBDA.

At steady-state conditions, the hot channel normalized flow should be equal to the radial peaking factor. When flow resumes after the 5-min adiabatic heatup, the hot channel normalized flow drops to less than 1.2. During the transient, the hot channel does not cool as fast as the other channels, and therefore, the normalized flow continues to decrease, primarily from feedback effects of temperature in calculating frictional losses. However, once the hot channel begins to cool down, the gradients between refueling regions start to collapse and the normalized flow in the hot channel slowly increases.

These normalized flows were used as input to the CCCM code to obtain a detailed temperature distribution.

15. AIR INGRESS RATES TO THE PRIMARY SYSTEM FOLLOWING A DBDA

R. L. Reid

The High-Temperature Gas-Cooled Reactor (HTGR) Safety Study Program for the Division of Reactor Licensing, U.S. Nuclear Regulatory Commission, was in need of a model to calculate the rate of air ingress into the prestressed concrete reactor vessel (PCRVR) following a design-basis depressurization accident (DBDA). General Atomic Company (GA) had developed a computer code to predict the air ingress rate,^{1,2} but it was deemed necessary to develop an independent code, to compare the results of both codes, and to provide a tool for analysis of any HTGR. The air ingress rates and resultant changes in effective molecular weight of the PCRVR gas are particularly important in the design of the core auxiliary cooling system.

15.1 DESCRIPTION OF THE DBDA PROCESS

Two types of ruptures in the PCRVR are considered: a break around the bottom of the steam generator penetration (point A in Fig. 15.1) where the hot helium enters the cavity (hot-leg break) or a leak near the top of the cavity (point B in Fig. 15.1) where the helium has been cooled and is returning to the core (cold-leg break). When the leak is initiated, a blowdown occurs from the PCRVR, beginning at the operating pressure [about 5 MPa (700 psia)] and ending at some low pressure. The blowdown changes pressure and temperature in the containment, which is initially near atmospheric pressure and ambient temperature, for a few minutes until relatively steady-state conditions are reached. The entire PCRVR and containment then equilibrate at a pressure of 0.14–0.21 MPa (20–30 psia), depending on whether the leak was a hot- or a cold-leg break. The containment temperature at this time is about 28–66°C (100–150°F).

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Fig. 15.1. Diagram of Core Cavity, Steam Generator Penetration of the PCRV, and the Containment Structure.

These pressure and temperature excursions are described and calculated by the Contempt-G computer program.³ For the present study, the long-term effects (those up to 6 hr) were of interest; therefore, the containment was assumed to reach the steady-state temperature and pressure instantly.

At least four types of mass transfer processes can occur during the DBDA to alter the effective molecular weight of the gas in the PCRV from a value of 4.00 for pure helium. These are (1) expansions and contractions due to temperature changes in the PCRV, (2) establishment of a free convection loop, (3) reaction of the incoming oxygen with carbon in the reactor to form carbon monoxide, and (4) diffusion.

The free convection loop will be a possibility only with the cold-leg break that occurs at the top of the steam generator. A stable buoyant situation exists with the hot-leg break at the bottom of the PCRV. Diffusion was determined to be negligible under both these conditions.

General Atomic's description of the calculations in Sect. 5.1.4 of LTR-1 (ref. 1) considered points 1 and 2 (see Fig. 1) above the

reactor core. Therefore, a cold-leg break was considered, although this was not stated. Conversations⁴ revealed that GA included the reaction of incoming oxygen with the graphite in the core to form CO; this point is not noted in LTR-1. These conversations actually led to the decision to include this reaction in the ORNL model.

15.2 COMPUTATIONAL MODEL⁵

15.2.1 Free Convection Loop

Most of the assumptions used by GA¹ were also made in this study, including the assumption that the total cross-sectional area available for inflow and outflow is 0.0645 m^2 (100 in.^2). The height of the buoyant column was taken as 2.4 m (8 ft) (the distance from C to D in Fig. 15.2, which was the effective height of the annular gap). Friction in the column was ignored, because the flow area away from the flow restrictor (point C in Fig. 15.2) was significantly greater than this minimum flow area. The major inhibitor of flow was assumed to be the entrance and exit losses at the flow restrictor. These were treated as a sudden contraction loss with a loss coefficient of about 0.5 and a

Fig. 15.2. Detail of Closure at Top of Steam Generator Cavity Showing Flow Restrictor and Annular Gap.

sudden expansion that can have a loss coefficient as large as 1.0, depending upon the ratio of flow areas. To be conservative (i.e., to overestimate the rate of air ingress), the sum of these two coefficients was assumed to be 1.2, which is the same assumption used by GA.¹

In the fundamental equation for the free convection loop, the difference in head produced by the different weights of the columns is balanced by the pressure losses in the flow path. With the assumptions discussed above, the equation is given by

$$K \frac{\rho_C V_C^2}{2} + K \frac{\rho_R V_R^2}{2} = Lg(\rho_C - \rho_R) , \quad (1)$$

where

V = velocity,

ρ = density,

K = loss coefficient,

L = height of column,

g = gravitational constant.

Subscripts C and R refer to conditions in the gas mixture in the containment and the PCRV respectively.

If Eq. (1) is converted to the volumetric flow rate Q by use of the cross-sectional area A , it becomes

$$K \frac{\rho_C Q_C^2}{2A_C^2} + K \frac{\rho_R Q_R^2}{2A_R^2} = Lg(\rho_C - \rho_R) . \quad (2)$$

However, $Q_C = Q_R$, and if the cross-sectional areas for inflow and outflow are assumed to be equal such that $A_C = A_R = A = 0.032 \text{ m}^2 = 50 \text{ in.}^2$,

$$Q = A \sqrt{\frac{2Lg(\rho_C - \rho_R)}{K(\rho_C + \rho_R)}} . \quad (3)$$

Transforming this to the mass flow M_R of the outflowing gas, the equation becomes

$$M_R = A \sqrt{\frac{2Lg(\rho_C - \rho_R)\rho_R^2}{K(\rho_C + \rho_R)}}. \quad (4)$$

Equation (4) can be used over a time interval Δt to determine the amount of gas leaving the containment.

15.2.2 Expansion-Contraction Process and CO Formation

The expansion-contraction process is essentially an inventory system using the ideal gas law. However, several possible sequences of events and conditions can be hypothesized. The reaction of oxygen with graphite to form CO is also incorporated into the procedure. The reaction is

The sequence of events in the expansion-contraction process with chemical reaction proceeds as follows:

1. Equation (4) is first used to calculate the mass of gas mixture leaving the PCRV in the time interval Δt . An equal volume of gas mixture from the containment enters at the containment temperature.
2. The oxygen in the incoming containment gas then reacts to form 2 moles of CO for each mole of O_2 .
3. The newly formed CO and the rest of the gas that came from the containment are then heated to the average PCRV temperature.
4. The difference in this expanded volume and the volume of the gas that left the PCRV is then assumed to displace an equal volume of gas from the PCRV to the containment.
5. The new average temperature at the end of the time step is used to calculate a new volume of PCRV gas.
6. If the temperature has increased, the amount of gas that must leave the PCRV to keep the pressure constant is calculated, and this amount is removed from the PCRV.

7. If the temperature has decreased, the volume of containment gas that will enter at containment temperature is calculated. The procedure then repeats steps 2 through 4.

8. The entire sequence is repeated starting with step 1.

A complete listing and documentation of the program INGRES is given in the Appendix of the report⁵ on INGRES.

15.3 RESULTS FROM THE MODEL

15.3.1 Summit and Fulton HTGRs

The computed results for the effective molecular weight of the gas in the PCRV and the helium mole fraction as a function of time are given in Tables 15.1 and 15.2 for the 2000- and 3000-MW(t) HTGRs. The input data in the tables were taken from the Summit and Fulton PSARs.^{6,7} These transient molecular weight distributions are plotted in Fig. 15.3.

Fig. 15.3. Molecular Weight of Gas in the PCRV for Summit [2000-MW(t)] and Fulton [3000-MW(t)] Reactors.

1

Time (hr)	Av Coolant Temperatures, °C(°F)		Molecular Weight	Mole Fraction He
	in	out		
0.0	319(607)	769(1416)	4.00	1.0000
0.0833	186(367)	827(1520)	6.05	0.9148
0.0892	187(368)	829(1524)	6.08	0.9132
0.1004	187(369)	831(1527)	6.16	0.9101
0.2012	189(372)	848(1558)	6.82	0.8827
0.3008	191(375)	863(1585)	7.43	0.8572
0.4004	190(374)	876(1609)	8.01	0.8331
0.5000	189(373)	889(1631)	8.55	0.8105
0.6064	190(374)	901(1653)	9.09	0.7878
0.8064	191(376)	920(1688)	10.05	0.7481
1.0064	197(387)	951(1744)	10.89	0.7129
1.2084	193(380)	961(1761)	11.65	0.6815
1.3954	194(382)	969(1776)	12.27	0.6555
1.5994	196(384)	975(1787)	12.87	0.6303
1.8034	197(386)	978(1793)	13.41	0.6080
2.0164	197(386)	978(1793)	13.90	0.5873
2.1964	197(387)	979(1794)	14.28	0.5718
2.3964	198(388)	978(1793)	14.65	0.5564
2.5964	198(388)	976(1789)	14.98	0.5427
2.7964	198(389)	972(1782)	15.27	0.5305
3.0364	198(389)	966(1771)	15.58	0.5175
3.3964	198(388)	953(1748)	15.98	0.5007
3.7964	197(387)	936(1716)	16.34	0.4857
4.1964	196(384)	916(1680)	16.63	0.4738
4.5964	193(380)	894(1641)	16.86	0.4643
5.0364	191(376)	869(1596)	17.06	0.4560
5.3964	188(371)	849(1560)	17.18	0.4507
5.7964	186(367)	826(1519)	17.30	0.4458
5.9964	184(364)	816(1500)	17.35	0.4438

Molecular Weight of Containment is now 17.26

15.3.2 Comparison with GA Results

The GA results² were actually obtained before some of the design conditions for the 2000- and 3000-MW(t) plants were finalized. Consequently, the GA results, although presented as being generic for both plants, are not exact for either reactor. In addition, LTR-1 (ref. 1) indicates a column height of 2.4 m (8.0 ft), but these older results were obtained for a column height of 5.64 m (18.5 ft). The results in Table 15.3 were obtained from input data that seemed to be the same as the GA data.

Peterson⁸ disclosed some differences between the calculational procedures followed in this report and the GA method. As reported in the LTR,¹ GA assumed that the total area for leakage 0.0645 m² (100 in.²) was divided into variable inflow and outflow areas. An equation similar to Eq. (3) then resulted; this equation was differentiated with respect to one of the flow areas, and the derivative was set equal to zero to produce an equation for the area to maximize the flow rate. As discussed earlier, the INGRES results were obtained by assuming a 50-50 split in the flow areas. A more detailed procedure seemed to be unnecessary considering the approximate nature of the rest of the analysis. The split to maximize the flow rate was close to the 50-50 mark.⁸

Another difference in the method was that the GA procedure coupled the free convection loop with the expansion-contraction process. If the outflow due to expansion was greater than the inflow due to free convection, no free convection was allowed to occur. As previously described, the analysis in this report considers the free convection to occur before and independent of the expansion-contraction process. This is more conservative and appears to be possible under certain actual local pressure and concentration variations in the PCRV and containment volume.

The results of this model are compared with the results of GA in Fig. 15.4. Note that, as discussed, these results seem to be conservative in comparison with the results of GA. In addition, the two sets of input data may differ slightly. Table 15.3 presents the computer results for this case.

Table 15.3. Comparison of Values from INGRES with Those of GA

Containment Pressure Before DBDA = 14.7 psia = 0.101 MPa
 Volume of Containment = 1,687,000 ft³ = 47,770 m³
 Temperature of Containment = 610.0°R = 338.9 K
 Mass of Helium in PCRV Before DBDA = 17,420 lbm = 7902 kg
 PCRV Pressure Before DBDA = 700.0 psia = 4.826 MPa
 System Pressure After DBDA = 23.2 psia = 0.160 MPa
 Maximum Cross Sectional Area for the Leak = 100.0 in.²
 = 0.0645 m²
 The Loss Coefficient for Combined Inlet and Outlet
 Losses for Each Column = 1.2
 The Height of the Buoyant Column = 18.5 ft = 5.67 m
 Molecular Weight of Containment Immediately After DBDA
 is 15.75

Time (hr)	Av Coolant Temperatures, °C(°F)		Molecular Weight	Mole Fraction He
	in	out		
0.0	319(607)	769(1416)	4.00	1.0000
0.0833	186(367)	827(1520)	5.92	0.9201
0.0892	187(368)	829(1524)	5.97	0.9179
0.1004	187(369)	831(1527)	6.07	0.9137
0.2012	189(372)	848(1558)	6.95	0.8770
0.3008	191(375)	863(1585)	7.74	0.8443
0.4004	190(374)	876(1609)	8.45	0.8146
0.5000	189(373)	889(1631)	9.10	0.7876
0.6064	189(373)	901(1653)	9.72	0.7615
0.8064	190(374)	920(1688)	10.78	0.7177
1.0064	191(376)	937(1719)	11.64	0.6815
1.2084	197(387)	951(1744)	12.38	0.6510
1.3954	193(380)	961(1761)	12.94	0.6273
1.5994	194(382)	969(1776)	13.47	0.6056
1.8034	196(384)	975(1787)	13.90	0.5874
2.0164	197(386)	978(1793)	14.28	0.5716
2.1964	197(387)	979(1794)	14.55	0.5604
2.3964	198(388)	978(1793)	14.81	0.5497
2.5964	198(388)	976(1789)	15.02	0.5407
2.7964	198(389)	972(1782)	15.21	0.5331
3.0364	198(389)	966(1771)	15.39	0.5254
3.3964	198(388)	953(1748)	15.61	0.5162
3.7964	197(387)	936(1716)	15.79	0.5087
4.1964	196(384)	916(1680)	15.92	0.5034
4.5964	193(380)	894(1641)	16.01	0.4996
5.0364	191(376)	869(1596)	16.08	0.4967
5.3964	188(371)	849(1560)	16.12	0.4950
5.7964	186(367)	826(1519)	16.16	0.4935
5.9964	184(364)	816(1500)	16.17	0.4930

Molecular Weight of Containment is now 15.58

Fig. 15.4. Comparison of INGRES Results with GA.

15.4 CONCLUSIONS

The analysis presented in this report seems to give reasonable results for the gas composition of the PCRV as a function of time following a DBDA. The results compare reasonably well with the independent calculations presented by GA.

15.5 REFERENCES

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16. PERFORMANCE OF THE THERMAL BARRIER COVER PLATES AT THE LIMITING TEMPERATURES

C. W. Collins and S. K. Iskander

The Nuclear Regulatory Commission requested the Oak Ridge National Laboratory to assist in evaluating a Licensing Topical Report (GA-LTR-1)¹ for High-Temperature Gas-Cooled Reactors (HTGRs). As part of this effort, two typical metal cover plates used with the insulated prestressed concrete reactor vessel were analyzed at severe transit conditions.

16.1 PROBLEM STATEMENT

At normal operating conditions, HTGRs use high-pressure helium, which is heated from a core inlet temperature of about 315°C (600°F) to an outlet temperature of about 760°C (1400°F), and even higher temperatures at upset and faulted conditions. To protect the concrete reactor vessel from these high temperatures, fibrous insulation is installed between the vessel and a metal cover plate that confines the insulation and exposes a smooth, clean inner surface to the flowing helium. Amounts of insulation and metal types and shapes differ throughout the pressure vessel.

Two types of cover plates were selected for analysis as being typical for the entire reactor. The first plate is a 6.4-mm (1/4-in.) Hastelloy X square plate, used in the outlet plenum. The second plate is a 6.4-mm carbon steel hexagonal plate, used in the inlet plenum. Both plates are supported at the center and at the edges. Pressure applied to the plates compresses the 100 mm (4 in.) of Kaowool to 76 mm (3 in.), and the plates are then attached to the supports, resulting in an initial maximum preload to the plates of 20 kPa (3 psi). The General Atomic Company (GA) tested Kaowool to determine the effects of temperature, time, irradiation, and different amounts of precompression. These data showed that the initial plate preload would decrease from 20 kPa (3 psi) to 5.2 kPa (0.75 psi) through loss of resiliency at elevated temperature for a relatively short period of time.

The topical report¹ (GA-LTR-1) gives the design bases for the core cooling system as determined by subjecting the core and PCRV internals to a set of hypothetical severe thermal transients to establish the conditions at which component damage could occur. For evaluating transient severity, two threshold limits for temperatures of important primary system components are defined:

Damage Limit -- Defined as that component temperature at which damage may be incurred to the extent that normal plant operation may not be resumed without repairs. "Damage" is defined as total creep deformation strain exceeding the design limit by 1%. For the square plate the damage limit was reported as 982°C (1800°F) for 10 hr; for the hexagonal plate as 538°C (1000°F) for 10 hr.

Critical Safety Limit -- Defined as that component temperature at which damage may be incurred to the extent that it would interfere with effective core cooling. Exceeding the "Critical Safety Limit" results in "failure," which is defined as total creep deformation strain exceeding the design limit by 3%. For the square plate the damage limit was reported to be 1093°C (2000°F) for 1 hr; for the hexagonal plate as 666°C (1230°F) for 1 hr.

In an additional failure of the thermal barrier system, the metal cover plates deflect excessively, so the insulating material is no longer compressed against the liner of the PCRV. Such failure could result in local excessive heat input to the PCRV cooling system and possibly a buckling failure of the liner in that area. Collapse of the thermal barrier itself could introduce debris into the coolant flow, blocking some flow passages. As the plate deflects under load, the initial uniform pressure due to the precompressed insulation is reduced. This relationship between the deflection of the plates and the pressure was assumed to be linear. This variable loading was taken into account by modifying the EPACA computer program, which was used to provide a finite element analysis for the elastic, plastic, and creep strains produced by the specified loading conditions.

16.2 RESULTS OF THE ANALYSIS

The results of the analysis for the square plate show that the maximum total strains (elastic, plastic, and creep) are 0.35% and 0.54% at 982 and 1093°C (1800 and 2000°F), respectively. Also, extensive inelastic yielding has permanently warped the plates, even under the small loads to which they were subjected. This is due to the very small elastic strength remaining in the plates at these high temperatures. The analysis shows that the free corners of the plate will displace more than 25 mm (1 in.) and thus lift off the insulation and fail to meet one of the design criteria. This occurred for both temperatures, the displacement of the free corner being 30 mm (1.2 in.) at 982°C (1800°F) and 41.4 mm (1.63 in.) at 1093°C (2000°F).

Results for the hexagonal plate revealed no such problem. In fact, the plate remained substantially elastic, and no inelastic strains occurred for the 2 1/4 Cr-1 Mo steel substituted in the analysis. The maximum equivalent stresses were about 70 MPa (10 ksi), sufficiently below the yield stress of 77.6 MPa (11.25 ksi) at the higher temperature level of 649°C (1200°F) for 1 hr.

The maximum deflection of the hexagonal plate was 10.9 mm (0.43 in.) and was, of course, for the 649°C (1200°F) temperature. Although the 2 1/4 Cr-1 Mo hexagonal plate was found satisfactory, it must be concluded that the integrity of the carbon steel plates at the specified conditions would be very doubtful.

A detailed report on the inelastic finite element analysis of the plate was completed and submitted to NRC for review.²

16.3 REFERENCES

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17. EVALUATION OF TEMPERATURE LIMITS FOR THE FAILURE OF FUEL PARTICLE COATINGS

F. J. Homan

In Table 2.2 of LTR-1 (ref. 1) both damage and critical safety limit temperatures related to the failure of fuel particle coatings are indicated. The damage limit is defined as the temperature at which damage may be incurred to the extent that normal plant operation may not be resumed without repair. In this instance, the damaged fuel would have to be replaced; this can be accomplished by normal refueling procedures. The critical safety limit is defined as that temperature at which damage may be incurred to the extent that it would interfere with effective core cooling.

The damage limit temperature is indicated as 1650°C (3000°F). Damage would be defined as reaching the temperature at which sufficient fuel particle coatings would fail that excessive amounts of fission products would be introduced into the primary coolant gas. Failures can occur in the short term if excessive fission gas pressures develop within the particle or excessive thermal stresses develop in the coating layers. These types of failures are quite dependent upon the total exposure of the fuel to irradiation and the amount of burnup of the fuel material plus the manner and temperature at which the coating layers were applied. These latter effects were discussed elsewhere.²

Long-term coating failure can occur from fuel particle migration into or through the particle coating layers. This effect has been reviewed in detail.³

The critical safety limit for the fuel particle coatings is reported¹ as greater than 2370°C (4300°F). This is the point at which the fuel element fails structurally; in particular, the fuel graphite matrix loses its structural integrity and fuel movement may occur. Fuel movement must occur at temperatures above the fuel melting point [$\geq 2370^\circ\text{C}$ (4300°F)] up to the point of graphite sublimation [$> 3300^\circ\text{C}$ (6000°F)].

17.1 FAILURE OF FUEL PARTICLES DUE TO RAPID THERMAL TRANSIENTS —
F. J. Homan

The 1650°C (3000°F) damage limit temperature appears to be acceptable. Thoria particles have operated successfully in HT experiments in the HFIR to temperatures this high. However, as stated by GA,¹ the temperature at which particles will burst will depend strongly on the detailed time-temperature history. This temperature will also depend on the amount of burnup accumulated by the particle. The HFIR HT particles were low-burnup particles (fertile). High-burnup fissile particles would develop much higher internal gas pressures during a temperature rise and would be more apt to fail. Reference 1 states (on page 2-15) that "particles at the highest burnup can be heated to temperatures around 1650°C (3000°F) for periods of hours without significant damage". If this has been done experimentally, that fact should be documented.

The 2370°C (4300°F) critical safety limit temperature is probably satisfactory, but the thinking that led to this value is not well described¹ (in Sect. 2.4). Loss of integrity of the fuel-graphite matrix is no problem, and it does not interfere with subsequent cooling. In fact, matrix crumbling has been observed in experimental fuels at much lower temperatures. Coolant does not flow through the gap between the fuel and the graphite, so the fact that the rod crumbles and may fill this gap does not result in a loss of the ability to cool the fuel. The last paragraph on page 2-15 of ref. 1 appears to be describing a situation where the fuel melts and eats through the coating very rapidly, resulting in a concentration of fissile fuel. This could then melt through the graphite block and cause blockage of the coolant channels.

17.2 PARTICLE FAILURE UNDER ISOTHERMAL CONDITIONS — H. J. de Nordwall
and T. B. Lindemer

The temperature at which an irradiated particle will fail in the absence of a temperature gradient or neutrons can be estimated only on a generic basis, and then with little statistical confidence. Specific data for large-HTGR reference fuel is not available to us.

Our observations were made on a total of about 60 loose particles. Eight of them were from a GA capsule (F-29) in which reference Fort St. Vrain fuel has been irradiated to full fluence and burnup (23% FIMA and 8.3×10^{21} n/cm²). The remainder were Triso-I particles; that is, they contained no inner LTI layer other than a seal coat — an unsatisfactory design.

Two types of failure were observed. In type 1, the particle became permeable to cesium and strontium but continued to retain krypton. In type 2, the particles exploded.

There is still some doubt whether type 1 failures were the result of a reversible change in SiC permeability to metals or whether the SiC was recrystallizing or cracking, either of which is irreversible. Evidence of both was eventually found. Dragon work by Voice⁴ showed that recrystallization occurred at lower temperature if free silicon was present, but it will occur anyway above 1700°C. Recrystallization is accompanied by void formation.

Since the SiC layer always "failed" first, it became clear that this must be the most highly stressed coating. This was subsequently confirmed by use of the STRESS-II computer code⁵ obtained from Dragon. Calculations aimed at predicting failure were unrewarding since the temperature dependence of coating mechanical properties were not (and are not) known. A coating failure stress could be chosen that permitted one to separate failed (type 1) from unfailed particles in the manner that is done in fuel design, but this was of little absolute value in this context. The most sensitive parameter was the buffer volume. Those particles whose buffer volume exceeded 1.7 kernel volumes survived 2000°C. Type 1 failures were observed⁶ above 1700°C.

Type 2 failures never occurred below 1800°C (1 particle, after 1 hr). Usually even the rudimentary Triso-1 particles survived stepwise heating to over 2000°C, when failure occurred⁷ in 1 to 18 hr.

We plan to continue these studies using fuel and fuel rods from Fort St. Vrain and irradiations of reference fuel so that statistically significant data might be obtained.

Biso particles, particularly those initially containing only ThO_2 would be expected to survive to high temperatures for moderate times. Unirradiated Biso-coated UO_2 has survived for 1000 hr at 2000°C .

Brown⁸ at Harwell has reported that large ($800\text{--}\mu\text{m}$ kernels) irradiated Triso-coated $^{235+238}\text{UO}_2$ particles were failing under 1500°C if they had been irradiated above 1450°C ; otherwise these fuels will survive 1750°C for several hours. No reasons were given. These fuels have higher oxygen potentials than $(^{233}\text{U},\text{Th})\text{O}_2$ fuels and relatively smaller buffer volumes, both of which contribute to higher internal pressures.

It must also be pointed out that since the mechanisms controlling the pressure of CO in an irradiated UO_2 particle are not clearly understood, the temperature coefficient of the internal CO pressure in a high-burnup oxide fuel (UO_2) cannot be calculated.

To summarize, our inability to make a good estimate of the isothermal failure temperature can generally be accounted for by the following:

1. we do not know the effects of particle-matrix mechanical interaction, but we expect them to be beneficial;
2. we cannot calculate type 2 failures, as we have no temperature-dependent mechanical property data;
3. we have not seen any experimental results for reference large-HTGR fuel from GA or elsewhere;
4. we do not know how nominal mechanical properties of reference fuel differ from fuels tested, or how real properties are distributed;
5. we are not certain whether fission product release through SiC will always be a result of an Arrhenius, and therefore reversible, dependence of diffusion coefficients upon temperature or a result of the irreversible formation of cracks across the SiC layer;
6. in short, we have essentially no statistical base for answering this question.

With these reservations, 1650°C for a type 1 failure may be a bit low. It is probably all right for a type 2 failure for 2000-MW(t) reference fuel, but not proven.

17.3 PARTICLE FAILURE BY KERNEL MIGRATION — H. J. de Nordwall and T. B. Lindemer

The fuel particle can fail by kernel migration through the coating under the influence of a temperature gradient as well as by isothermal causes. We estimate that the temperature gradient after core shutdown and under decay heat conditions will be 3 to 10% of that at full power; that is, the ratio of temperature gradient after shutdown to that at full power is roughly the same as the fraction of power after shutdown. This gradient after shutdown is estimated to be somewhere between 20 and 60°C/cm.

The temperature for failure by kernel migration can probably be best estimated by comparing values of the kernel migration coefficient (KMC) for specific oxide and carbide fuels with the approximate limiting value of the kernel migration coefficient *established by the reactor conditions*, as is done in Fig. 17.1. The reactor contains fuel with a

Fig. 17.1. Kernel Migration Coefficient Versus $1/T$ for Biso-Coated Particles. The UC_2 behavior was established by GA, while the ThO_2 behavior was established by ORNL. Original figure from GA.

coating that is about 200 μm thick; therefore, kernel migration of 200 μm will certainly result in loss of fission product containment. If we arbitrarily assume that this migration can take place in 30 days, then the reactor-established migration rate is 200 $\mu\text{m}/30$ days. The reactor-established KMC is calculated by dividing this rate (in $\mu\text{m}/\text{sec}$) by the temperature gradient of 20 or 60°C/cm and multiplying by T^2 (in K^2) to give the nearly horizontal band shown in the upper portion of Fig. 17.1. We see that the least squares average behavior of the KMC for UC_2 particles⁹ intersects this reactor-established band somewhere between 1950 and 2050°C, while the 95% confidence limit intersects this band between 1750 and 1850°C. Also, the KMC for ThO_2 particles¹⁰ would intersect the band at even higher temperatures. Since the reference core will probably contain ThO_2 fertile and UC_2 fissile particles, the 1650°C ($\sim 3000^\circ\text{F}$) "damage limit temperature" in Table 2.2 of ref. 1 appears reasonable as far as kernel migration is concerned.

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18. ANALYSIS OF TRITIUM DISTRIBUTION IN HTGR

E. L. Compere

An analysis of the production, distribution, and release of tritium in HTGRs has been developed, computerized, and reported.¹ The transfer of tritium is described by Fig. 18.1, and the abstract of the report follows.

Tritium will be produced in high-temperature gas-cooled reactors not only by fission, but also to a significant extent by reactions of neutrons with boron in burnable poison and control rods, with trace lithium impurity, and with ^3He occurring naturally in the helium coolant flowing through the core. A portion of the tritium that is formed in solids or fuel may be released to the coolant. This tritium mingles with the hydrogen in the coolant and will, in part, chemisorb on graphite. In addition, it may leak with the coolant from the reactor vessel, be removed in the helium purification system, or permeate system walls into the steam generator, where as a result of removal (blowdown) or losses it is subject to release to the environment.

The differential equations describing the above behavior are developed. Assumption of steady conditions, including hydrogen concentration, for a period makes it possible to express the distribution as well as the production in the form of coupled sets of linear first-order ordinary differential equations. A generalized analytical solution of these is presented.

The above tritium generation and distribution model has been incorporated in a computer code, TRITGO. An illustrative calculation using parameters taken from the PSAR description of the Fort St. Vrain Reactor has been made. This indicates that most of the tritium emerging from the primary coolant will have been generated by the $^3\text{He}(n,p)\text{T}$ reaction. A significant fraction enters the steam generator. The tritium in the steam generator blowdown, after proper dilution, may be considered for release to the environment. The effects of varying a number of system parameters are considered.

Fig. 18.1 Pathways by Which Tritium is Transferred in HTGR Regions.

18.1 REFERENCE

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19. ENERGY RELEASE TO PCRV DURING DBDA

G. Samuels

The maximum amount of energy transferred from inside the PCRV to the containment system during the DBDA as reported by GA in Table 19.1 has been evaluated. The simplest approach is to treat the problem as one of energy transport rather than energy transfer; that is, what is the maximum amount of energy that can be transported from the PCRV internal by the helium inventory?

The maximum amount of helium in the PCRV is the nominal inventory of 6668 kg (14,700 lb) plus an uncertainty of 8% or a total of 7201 kg (15,876 lb). The internal energy of the helium inventory is given as 9.5 GJ (9×10^6 Btu), which corresponds to an average temperature of 423°C (794°F). The temperature of the helium leaving the PCRV during the blowdown will increase with time. The initial temperature will be that of the gas in the lower steam generator cavity, while the final temperature will depend on the number of circulators operating and the temperature of the lower section of the core and the lower reflector. A detailed discussion of the expected helium temperature leaving the PCRV is given in the Summit PSAR¹ and appears to be a valid analysis of the exit temperature during the transient.

The amount of energy that can be transported from the PCRV by the entire helium inventory during the blowdown is shown in Table 19.2. The helium inventory is held constant at 7201 kg (15,876 lb), and both the initial helium internal energy and the average exit temperature are varied. Using the GA estimate of initial internal energy [9.5 GJ (9×10^6 Btu)] and assuming the estimate by GA of the maximum exit temperature [769°C (1416°F)] to be the average for the entire blowdown, the amount of energy that can be transported to the containment system by the entire inventory is 12.9 GJ (12.2×10^6 Btu).

Thus, the maximum amount of energy that can be transferred from inside the PCRV to the containment system is limited by the helium inventory and the average temperature of the helium leaving the PCRV.

Table 19.1. PCRV Energy Balance for a Hot-Leg DBDA

	Value at $t = 0$	Transferred During 0-100 sec	Value at 100 sec	Transferred During 100-150 sec	Value at 150 sec
PCRV Helium Inventory, kg (lb)	7201(15,876) ^a		835(1842)		510(1124)
Energy, GJ (Btu $\times 10^6$)					
Primary helium internal	9.5(9) ^a		~1(1)		~0.6(0.6)
Added by core heat generation		46(44)		3.8(3.6)	
Added by main loop circulator, approximate		1(1)		0	
Approximate net sensible heat released from core and reflectors		11(10)		2 ^b	
Transferred to steam generator secondary		46(44)		~0	
Lost to PCRV liners, CACS, etc. ^c		0		0	
Maximum available for release ^d to the containment from 0 to t			20(19)		25(24)
Assumed release to the containment from time 0 to t			30(28) ^a		31(29) ^a

^aBased on nominal inventory plus 8%.^bEstimate.^cNeglected for conservatism.^dInitial internal energy of helium less current value at t plus net energy added to the primary coolant during the time interval.

Table 19.2. Energy Transferrable from the PCRV to the Containment System by 7201 kg (15,876 lb) of Helium

Average Temperature of Helium Leaving PCRV		Energy, GJ (10^6 Btu) Transferrable for Each Initial Internal Energy in GJ (10^6 Btu)		
(°C)	(°F)	8.4 (8)	9.5 (9)	10.5 (10)
746	1375	13.8 (13.1)	12.0 (11.4)	10.3 (9.8)
760	1400	14.3 (13.6)	12.6 (11.9)	10.9 (10.3)
774	1425	14.9 (14.1)	13.1 (12.4)	11.3 (10.7)
788	1450	15.4 (14.6)	13.6 (12.9)	11.8 (11.2)

Any additional heat generated within the core or added by the circulators is either stored within the PCRV or removed by the steam generators. The values reported by GA in Table 19.1 agree well with those found by the above analysis. There does not appear to be any reason for adding the 1 GJ (1×10^6 Btu) from the circulator during the first 100 sec or the 3.8 GJ (3.6×10^6 Btu) from core heat generation during the time from 100 to 150 sec.

Although the arguments presented above do not refer directly to the number of circulators in operation, the assumption that the entire inventory reaches the maximum exit temperature implies that the inventory passes through the core. For this to occur, all circulators must be operating or the inoperative loops must be blocked in some manner to prevent backflow. Considering that the normal flow through each loop is 236 kg/sec (520 lb/sec) and that the maximum unrestricted flow from the 0.065-m² (100-in.²) penetration is 156 kg/sec (345 lb/sec), all circulators may quite possibly continue to operate during the blowdown. With all circulators in operation, the temperature of the helium leaving the PCRV will be less than the 769°C (1416°F) peak reported by GA for only one circulator operating.

The total amount of heat transferred from the core to the helium will depend on the number of circulators operating. However, the only effect of this heat transfer on the amount of energy transferred to the containment system will be its effect on the exit gas temperature from the core. In this case, the higher the flow and the heat transfer rate the lower the exit gas temperature to the containment system. The extra heat transferred from the core by the recirculation will be transferred to the steam generators.

19.1 REFERENCE

1. Delmarva Power and Light Co., *Preliminary Safety Analysis Report, Summit Power Station Units 1 and 2*, DOCKET-50-450. (Note particularly "Response to AEC questions, Q6.30.")

PART III. HTGR SAFETY STUDIES FOR THE DIVISION OF REACTOR SAFETY RESEARCH

J. P. Sanders, Program Manager

Funding for the HTGR Safety Studies for the Division of Reactor Safety Research (RSR) began in July of 1974 and continued through the remainder of this reporting period. Similar programs of a much larger scope were developed during this same period at Brookhaven National Laboratory (BNL) and Los Alamos Scientific Laboratory (LASL); work at ORNL was coordinated with these efforts. One of the original efforts at ORNL was to evaluate certain computational methods that had been developed by the General Atomic Company (GA) for the evaluation of transient behavior in HTGR plants. In particular, attention was given to the following GA computer programs:

1. the BLOOST code, which was a point neutronic kinetics calculation coupled with a heat transfer analysis in the core;
2. the POKE code, which calculated certain flow conditions in the core;
3. the OXIDE code, which evaluated the effects of oxygen-containing compounds in the primary circuit;
4. the TAP code, which evaluates conditions in the primary and secondary systems during a plant transient; and
5. the RECA code, which evaluates conditions in the primary system during emergency operation.

An interval of time existed between the assignment of the project and the transfer of FORTRAN listings of these programs from GA. During this period, computer programs were written at ORNL to perform calculations similar to those incorporated in the indicated GA codes. These programs were based on the design descriptions of the large HTGRs, the equations of conservation and mass, and the general descriptions of the functioning of the GA codes from available reports. The purpose of this program development at ORNL was to provide comparative computational techniques to be used in evaluation procedures after the GA codes were available and to evaluate for ourselves those aspects that would be most difficult to model analytically. Our major interest was in the TAP and RECA programs, since these codes provide analysis of major segments of the plant during accident transients.

From past experience with similar types of analyses for other reactor plants, it appeared that the steam generator, which is the major interface between the primary and secondary systems, would require the most study to obtain a useful, meaningful, and accurate model. Because of this experience with modeling steam generators and in comparing these models with operational data, T. W. Kerlin of the University of Tennessee was given a subcontract to conduct studies of various analytical models for the HTGR steam generators. This work was performed principally by graduate students under his direction.

Computer programs were released by GA under a proprietary agreement with the three installations, ORNL, BNL, and LASL. The normal procedure was to release the code and documentation through BNL with permission to transfer the information to the other two sites. The General Atomic Company has a Univac computer system with a FORTRAN V compiler. Both BNL and LASL have CDC systems, and ORNL has an IBM-360 system with a FORTRAN IV compiler. Because of differences in the computers, the compilers, and the executive systems on the computers, none of the programs could be transferred for operation at another installation without appropriate modification.

During the period before the codes were received, modeling of the most important segments of the codes continued at ORNL. This included a RECA-type model of the core (a multichannel model) and a TAP-type model of the core (a single-channel model). In addition, a detailed model of the steam generator was written, and the turbine-generator plant was simulated by modifying subroutines previously written for a desalination plant. The core auxiliary cooling system for large HTGR plants was also modeled.

20. CORE SIMULATION FOR EMERGENCY COOLING ANALYSIS

S. J. Ball

So we could better understand the behavior of the HTGR core thermal dynamics during emergency cooling operations, a computer code similar to the GA RECA (Reactor Emergency Cooling Analysis) code¹ was developed. The ORNL simplified counterpart of RECA, called ORECA, consists of three versions: one version for the Fort St. Vrain (FSV) reactor, another for the 2000-MW(t) Summit Power Station, and the third for the 3000-MW(t) Fulton Generating Station. Of the three, the FSV version was developed in the most detail. This was done to study design-basis depressurization accident (DBDA) transients that were of interest to the licensing group in NRC. Until the GA RECA code is operational on the local IBM computers, direct comparisons of RECA and ORECA predictions for a specific set of accident conditions are the most effective way of evaluating RECA and ORECA.

The present FSV version of ORECA has the following characteristics and capabilities:

1. A one-node approximation is used for each of the 37 fuel and 18 side reflector blocks in an axial slice for each of 8 axial regions, including top and bottom reflector and supports. (Total = 440 nodes).
2. Coolant heat transfer coefficient calculations include the effects of changing flow regimes (turbulent-transition-laminar), and the effects of helium conductivity variations with temperature.
3. Present inputs include the total helium flow rate, pressure, and inlet temperature vs time and the total reactor power input vs time, which follows a typical scram curve. Axial and radial power peaking factors are input and assumed constant throughout the run. Core specific heat and conductivity are also assumed constant.
4. The flow calculated for each channel depends upon friction losses, acceleration losses, buoyancy effects, and entrance and exit orifice pressure drops. Friction losses calculated depend on the flow regime.

5. Channel flows are calculated at each time step by an iterative scheme to solve for the overall core pressure drop, which gives the proper total flow rate (within a specified error). Reverse flows are also accommodated.

6. The typical IBM 360/91 computer time for a 7-hr transient using 5-min computation time steps is approximately 5 sec.

A number of specific accident cases have been studied, including the DBDA and the loss-of-coolant accident (LOCA) at pressure. Figure 20.1 shows preliminary results of a simulated FSV reactor depressurization accident. The reference case assumed that the reactor was scrammed from 105% power at the same time that pressure and flow were lost. After 5 min, it was assumed that the circulators were started (by use of the Pelton wheel drives) and that the core flow and inlet temperatures followed curves as supplied by GA from its TAP code. It was also assumed that the initial regional flow distribution had been adjusted so that the outlet temperatures from all regions were equal during full-power operation and that the effective adjustable-orifice coefficient was constant at the lower mass flows.

The maximum core and coolant outlet temperatures shown in Fig. 20.1 are 1282 and 1232°C (2340 and 2250°F), respectively, and the mixed-mean coolant outlet temperature reached 1010°C (1850°F). [Subsequent calculations incorporating core specific heat and conductivity as functions of temperature gave corresponding maximum temperatures of 1221, 1066, and 910°C (2230, 1950, and 1670°F)].

Several variations on the reference case were also run to determine the sensitivity of the results to certain assumptions about the input values. Some experimental data indicate that the effective flow coefficients of orifices would increase at very low flow rates; this would result in a more uniformly distributed flow. The effect was approximated by making the initial flow distribution midway between the reference distribution and equal distribution. This change resulted in a 56°C (100°F) increase in maximum core temperature. The assumption of a 20% reduction in the value of core composite conductivity [from 17 to 14 W/m K (10 to 8 Btu hr⁻¹ ft⁻¹ °F⁻¹)] increased the peak core temperature

Fig. 20.1. FSV Reactor Depressurization Accident Calculations — ORECA Code.

by only 17°C (30°F). When the value of $\bar{Q}/\rho C_p$ (the average region heatup rate with no cooling) was made 20% higher than the reference case, the calculated maximum core temperature was 50°C (90°F) higher.

The FSV reactor version of ORECA was also used to predict the system response to a 30-min LOCA as reported² in the FSAR. Figure 20.2 compares the ORECA and the FSAR (Fig. 14.4-7) results, which are quite similar in spite of numerous differences in models and assumptions. The emergency cooling flow, initiated after 30 min, is assumed to be 20.4 kg/sec (162,000 lb/hr), driven by only one of the four available circulators. The maximum fuel temperature of 1088°C (1990°F) predicted by ORECA occurs in a region with a 1.78 radial power peaking factor and a 1.33 axial peaking factor (just above the midplane). Resumption of cooling flow carries the heat toward the outlet and provides for rapid cooling near the inlet for all channels. Note that in both models, the

Fig. 20.2. Response of ORECA and FSAR Predictions of FSV Reactor Response to a 30-min LOCA.

maximum core channel gas outlet temperature exceeds the maximum fuel temperature during cooldown. This is due to the gas being heated up by the lower reflector and support blocks, which are not considered to be in the "fuel" region. Also, about 45 min after cooling is resumed, flow reverses in several channels that have very low power peaking factors. This causes a slight temporary upturn in maximum fuel temperature (which shifted to the low-power regions) but only at a relatively low temperature [788°C (1450°F)]. Persistent (local) channel flow starvation with ensuing high fuel temperatures could occur if certain unfavorable coolant flow transients were imposed. Otherwise, with steady flows, the situations appear to be self-correcting.

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21. CORE MODEL FOR USE IN OVERALL NUCLEAR STEAM SUPPLY TRANSIENT ANALYSIS

J. C. Cleveland

A coupled heat transfer and neutron kinetics core simulation code for the HTGR core has been developed. The code has been incorporated into an overall nuclear steam supply system response program. The core model simulates an average fuel rod and the surrounding graphite moderator and coolant channels over the entire axial length of the active core. The axial power distribution is input to the code through axial power shape factors. The time-dependent temperature distribution in the fuel and moderator is determined by solving the transient heat conduction equation. Heat transfer to the coolant is determined through a calculated convective heat transfer coefficient that depends on whether the helium flow is turbulent, transitional, or laminar. Heat transfer to the coolant from the upper and lower reflectors and the core support block is also modeled. Furthermore, the heat transfer to the coolant flowing through the side reflectors and to the coolant flowing through the control rod and reserve absorber channels is modeled, and the mixed mean core outlet helium temperature is calculated from the relative flows through the main coolant channels, the side reflector elements, and the control rod and reserve absorber channels. The code also has the capability to calculate the transient temperature distribution in the "hot" fuel rod and the surrounding graphite and the coolant temperature in the adjacent coolant channels for cases where these are of interest. Nonlinear effects introduced by temperature-dependent fuel and moderator densities, specific heats, thermal conductivities, and helium viscosity and conductivity are included in the model.

The neutron kinetics behavior of the core is modeled by use of the space independent neutron kinetics equations with six groups of delayed neutrons. Fuel and moderator temperature coefficients of reactivity are required as input and may be temperature dependent. A much smaller computational time step is used for the solution of the neutron kinetics equations than is used for the solution of the heat transfer equations

because of the relatively long response times of the fuel and moderator temperatures. Average fuel and moderator temperatures are required to determine the feedback reactivity from the temperature-dependent reactivity coefficients. However, the feedback reactivity determines the core power level, which in turn affects the fuel and moderator temperatures. Therefore, the core model couples the neutron kinetics equations with the heat transfer equations through an iterative procedure requiring convergence on feedback reactivity.

The core simulation code is fully operational, and results have been compared with other calculations. Figure 21.1 shows computed temperature profiles at steady-state, 100% power for the Delmarva reactor core compared with results reported by General Atomic (GA).¹ Each of the circles in Fig. 21.1 representing the fuel centerline and maximum graphite temperatures calculated by the (ORNL) core simulation code should be considered as representing averages over each one-eighth of the active core height. Considering this, the results compare favorably with those reported by GA.

Results for both steady state and transient conditions obtained by using the ORNL core model to simulate the Fort St. Vrain reactor core have been compared with results obtained at ORNL using BLOOST-5, a GA coupled heat transfer, neutron kinetics code.² Both codes model the core with a single average channel and represent the neutron kinetics with the space-independent model. Therefore, they can be expected to give quite similar results. The steady-state comparison was for 100% power. Although a much finer mesh approximation was used for the BLOOST-5 calculations, the two codes agreed to within less than 6°C (10°F) on fuel centerline temperature in the average fuel stick. [Fuel centerline temperatures range from about 565 to 900°C (1050 to 1650°F) in the average fuel column, depending on the axial location].

The transient case considered for the BLOOST-5 comparison was a 1-cent step increase in reactivity at full power conditions. "Beginning-of-cycle-one" neutron kinetics parameters and reactivity

Fig. 21.1. Comparison of GA and ORNL Steady-State Axial Core Temperature Distributions.

coefficients were used. Figures 21.2, 21.3, and 21.4 compare the normalized power, core excess reactivity, and average channel outlet temperature obtained by the two codes. As can be seen, the agreement is quite favorable. The above comparisons provide an analytical verification of the core simulation code.

Fig. 21.2 Fort St. Vrain Core Power Response to 1-Cent Reactivity Step.

Fig. 21.3. Fort St. Vrain Excess Reactivity Response to 1-Cent Reactivity Step.

Fig. 21.4. Fort St. Vrain Average Channel Outlet Temperature Response to 1-Cent Reactivity Step.

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22. HTGR REHEATER AND STEAM GENERATOR MODEL DEVELOPMENT

R. A. Hedrick and J. C. Cleveland

A reheater-steam generator dynamic simulation code has been developed and incorporated into an overall nuclear steam supply system response program. The reheater-steam generator module is simulated by a multinode, fixed-boundary, homogeneous-flow model. The code solves the conservation of energy, mass, and momentum equations for both helium and water, and the conservation of energy equation for the tube as follows:

Helium and water nodes

Conservation of mass:

$$\begin{array}{lll} \frac{dM}{dt} & = & \dot{W}_{in} - \dot{W}_{out} \\ \text{(rate of change} & \text{(mass flow rate} & \text{(mass flow rate} \\ \text{in nodal mass)} & \text{into node)} & \text{out of node)} \end{array} \quad (1)$$

Conservation of energy:

$$\begin{array}{lll} \frac{dU}{dt} & = & (\dot{H}_{in} - \dot{H}_{out}) + C(T_{wall} - T_{fluid}) \\ \text{(rate of change} & \text{(net rate of heat} & \text{(rate of heat} \\ \text{of nodal} & \text{flow into node} & \text{transfer via convection} \\ \text{internal energy)} & \text{via transport)} & \text{and conduction)} \end{array} \quad (2)$$

Conservation of momentum:

$$\begin{array}{lll} L \frac{dW}{dt} & = & 144 g_c A (P_{in} - P_{out}) - \rho g A (z_{out} - z_{in}) \\ \text{(rate of change of} & \text{(net force on fluid in} & \text{(gravitational force} \\ \text{momentum of fluid} & \text{node due to pressure} & \text{on fluid in node)} \\ \text{in node)} & \text{difference)} & \end{array} \quad (3)$$

$$+ \left(\frac{W^2}{\rho A} \Big|_{in} - \frac{W^2}{\rho A} \Big|_{out} \right) - f_{Re} \left(\frac{12L}{D_H} \right) \frac{|W|W}{2\rho A}$$

$$\begin{array}{ll} \text{(rate of momentum} & \text{(drag due to} \\ \text{inflow - rate of} & \text{frictional force)} \\ \text{momentum outflow)} & \end{array}$$

Tube nodes

Conservation of energy:

$$M_{\text{tube}} c_p \frac{dT_{\text{tube}}}{dt} = C_{\text{He-tube}} (T_{\text{He}} - T_{\text{tube}}) - C_{\text{tube-water}} (T_{\text{tube}} - T_{\text{water}})$$

(rate of change of internal energy in tube node) (rate of heat transfer from helium by conduction and convection)

(rate of heat transfer to water by conduction and convection)

As many as 20 water nodes, 20 tube nodes, and 20 helium nodes may be used to represent the four sections — reheater, economizer, evaporator, and superheater — of the steam generator module. The code allows for a given helium node to transfer heat to more than one water node. For this case, the above equations are modified appropriately.

The convective heat transfer coefficient on the helium side is determined from the modified Grimson correlation¹ for helium flow over banks of tubes. Various correlations are used to determine the heat transfer coefficient on the water side depending on the water flow regime. These correlations are listed below:

<u>Regime</u>	<u>Correlation</u>
Subcooled forced convection	Dittus-Boelter
Two phase	
Nucleate boiling	Thom ²
Transition boiling	McDonough-Milch-King ³
Film boiling	Dougall-Rosenhow ⁴ or Groenveld ⁵ or Morgan ⁶ (for low flow)
Superheat forced convection	Dittus-Boelter
Departure from nucleate boiling (DNB) flux	W-3, ⁷ Hansen-Levy, ⁸ B&W-2, ⁹ or Barnett ¹⁰

The correlations for departure from nucleate boiling (DNB) flux, also given in the list, are used as trips to determine when two-phase correlations beyond DNB rather than the nucleate boiling correlation should be applied. Trips from other flow regimes are based on quality (e.g., from film boiling to superheat forced convection) or on a comparison of tube wall to bulk fluid temperature drop as calculated by correlations for adjacent regimes. For example, if a Dittus-Boelter correlation in the subcooled forced-convection regime results in a tube wall to bulk fluid temperature drop greater than the Thom nucleate boiling correlation, but the departure from nucleate boiling flux has not been exceeded, the nucleate boiling heat transfer correlation is used.

The friction factor calculation on the helium side is based on a combination of the correlations given by Kreith¹¹ for flow across in-line and staggered tubes. Friction factors on the water side are obtained from fits to the Fanning friction factor curves.¹² These are modified in boiling regions based on multipliers given by Thom.^{2,13}

The state variables in the above conservation equations are the mass, internal energy, and flow rate in each fluid node and the temperature of each tube node. The nodal pressure and temperature of each fluid node are considered to be functions of nodal mass and internal energy: that is,

$$P = P(M, U)$$

$$T = T(M, U)$$

The equation of state routines determine the nodal pressures and temperatures from the nodal mass and internal energy.

The conservation equations for the fluid and tube nodes can be written in state variable form as

$$\dot{\vec{y}}(t) = \vec{f}[\vec{y}(t)] , \quad (5)$$

where $\vec{y}(t)$ is a column vector whose components are the state variables and $\vec{f}[\vec{y}(t)]$ is a vector whose components are the right-hand sides of the conservation equations. For example, suppose y_n , the n th component of $\vec{y}(t)$, is the internal energy, U_j , of the j th helium node. Then f_n , the n th component of $\vec{f}$, would be the right-hand side of the following equation:

$$\frac{dU_j}{dt} = H_{in,j} W_{j-1} - H_{out,j} W_j + C_{He-tube} (T_{tube} - T_{He}) , \quad (6)$$

where

W_{j-1} = the flow rate in the upstream node,

note that f_n itself is a function of state variables.

By use of the integration technique presented in ref. 14, the value of the n th state variable — that is, the n th component of $\vec{y}$ — at time $t + \Delta t$ is written in terms of known values of state variables at time t by a linearized implicit formulation:

$$y_n(t + \Delta t) \approx y_n(t) + (\Delta t) \frac{dy_n(t + \Delta t)}{dt} , \quad (7)$$

where, using Eq. (5)

$$\frac{dy_n(t + \Delta t)}{dt} = f_n[\vec{y}(t + \Delta t)] \approx f_n[\vec{y}(t)] + (\Delta t) \frac{df_n[\vec{y}(t)]}{dt} ,$$

Applying the chain rule to the $df_n[\vec{y}(t)]/dt$ term gives

$$\frac{dy_n(t + \Delta t)}{dt} \approx f_n[\vec{y}(t)] + \Delta t \sum_{m=1}^M \frac{\partial f_n[\vec{y}(t)]}{\partial y_m} \frac{dy_m(t)}{dt} , \quad (8)$$

where M , the total number of state variables, is determined by the number of nodes used in the simulation. Applying the approximation

$$\frac{dy_m(t)}{dt} \approx \frac{y_m(t + \Delta t) - y_m(t)}{\Delta t}$$

and substituting Eq. (8) into Eq. (7) gives

$$\Delta y_n - (\Delta t) \sum_{m=1}^M \frac{\partial f_n[\vec{y}(t)]}{\partial y_m} (\Delta y_m) = (\Delta t) f_n[\vec{y}(t)] , \quad (9)$$

where

$$\Delta y_n \equiv y_n(t + \Delta t) - y_n(t).$$

This equation can be written in terms of the Jacobian matrix, $\underline{J}[\vec{y}(t)]$, as

$$I - (\Delta t) \underline{J}[\vec{y}(t)] \Delta \vec{y} = (\Delta t) \vec{f}[\vec{y}(t)] ,$$

where I is the unit matrix, and the lm th element of $\underline{J}$ is $\partial f_e / \partial y_m$. The code obtains the solution for $\Delta \vec{y}$ from the known state at time t . The values of all state variables at time $t + \Delta t$ are then obtained from

$$\vec{y}(t + \Delta t) = \vec{y}(t) + \Delta \vec{y} .$$

The data given in GA-LTR-5¹ were used to generate a base case for evaluating the model. In comparison with this base case, the steady-state search appears to be functioning properly and is fully operational. Transient calculations using the reheater-steam generator simulation code have also been successfully executed.

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J. G. Delene

The simulation of the Delmarva turbine-generator plant was checked out for transient operation. A 1%/sec ramp decrease in electrical demand was used as a test case. The code was found to operate satisfactorily for a least 75% reductions in demand at the 1%/sec rate. ("Rapid" load decreases are typically only 5%/min.) The code is now ready to be incorporated into the overall nuclear steam supply systems code.

Fig. 23.1. Flow Diagram of the Delmarva Summit Reactor Turbine-Generator Plant.

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24. CORE AUXILIARY COOLING SYSTEM MODEL DEVELOPMENT

L. G. Johnson* and S. J. Ball

Digital computer codes were developed to simulate the dynamics of the Core Auxiliary Heat Exchanger (CAHE) and the cooling tower emergency heat dump system. The Delmarva Summit reactor design was chosen as reference cases. In the present Delmarva design, the heat dump is a dry-air Core Auxiliary Cooling Tower (CACT). A detailed description of the CACT was obtained from the proposed vendor, the Marley Company, and was used as the basis for the model. Information from the PSAR¹ was used to develop the CAHE model.

Several variations of computer programs for both the CAHE and CACT were written and are operational. Final debugging is in progress. The several versions are being used to evaluate alternative numerical and modeling techniques. Sensitivities of the predicted responses to variations in the number and arrangement of nodes and to the size of computation time steps were investigated.

REFERENCE

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25. STEAM-GENERATOR DYNAMICS MODELING*

T. W. Kerlin

We are comparing and evaluating the spectrum of techniques presently used to predict once-through steam generator dynamic behavior. Preparations are also being made for steam generator and other reactor component model verification tests.

The steam-generator dynamics modeling has concentrated on several approaches: both linear and nonlinear moving boundary models and a nonlinear, fine-mesh fixed-node model. We compared linearized and nonlinear moving-boundary models and noted the sensitivity of the predicted responses to variations in parameter values and modeling assumptions. Evaluations have made use of GA's LAP code and a program developed by the Stone and Webster Company for the Fulton Generating Station.

In the development of the nonlinear fixed-node program, the economizer portion has been completed, and work has begun on the evaporator section.

The model verification tests are to be run on the Fort St. Vrain reactor. Most of the work on pretest analysis using the GA LAP (Linear Analysis Program) and on designs of several test procedures is complete. Further studies involving nonlinear models of FSV components are being made to compare with large-transient (e.g., scram) data. Work has also begun on preparing test procedure documentation for approval by the vendor and utility and on preparing automated data analysis and interpretation codes.

*University of Tennessee Subcontract.

26. DIGITAL COMPUTER PROGRAM ACQUISITION AND EVALUATION

J. P. Sanders

Negotiations were initiated during the last quarter of FY 1974 between Donald Schweitzer of Brookhaven National Laboratory (BNL), representing the AEC Division of Reactor Safety Research (RSR), and General Atomic Company to secure various GA codes for use in HTGR safety studies. Both ORNL and Los Alamos Scientific Laboratory (LASL) have been included in this effort so that RSR-supported programs at these installations could benefit by access to these computer programs.

The computer programs that were of interest to the three installations fell into two categories. One group constituted codes that had previously been released at some level of development to the general public through the Argonne Code Center. For these codes, GA indicated that the versions as released should be sufficient to satisfy the needs of the various installations and that subsequent modifications of these codes were too preliminary to justify their release outside the company.

The second group consisted of codes that, in their most recent modifications, had been developed through support by private funds. These codes would be released to the three installations only on a proprietary basis and only on the conditions that this status be maintained. To reduce the level of effort involved in making this group of codes available, GA asked that the official release be made to only one of the three interested installations and that subsequent distribution be made from that installation with the expressed approval of GA. BNL was the installation that was to receive these codes.

26.1 COMPUTER CODE NEEDS AT ORNL

The two codes of primary interest to our systems analysis work were in the above latter group. These two codes are Transient Analysis Program¹ (TAP) and Reactor Emergency Cooling Analysis² (RECA).

The TAP code simulates the dynamics of the entire plant, including the turbine-generator plant. It is particularly useful in following the effects of changes in load, plant startup, and shutdown. The primary loop components in this calculation are represented by a greatly simplified model. The RECA code presents a much more detailed model of the reactor core thermal and hydraulic calculations. It is most useful in the investigation of the consequences of the loss of forced convection and the consequences of the depressurization accident.

ORNL has requested that the LAP code³ be made available on a proprietary basis to serve until the TAP code is received and implemented. The LAP code, which was not included in our original requests for access to GA codes, is a linearized version of the TAP code. In the LAP code, the response of some of the major plant components is represented by a linearized function. The code is particularly useful in performing parametric studies of plant variables.

A version of the LAP code is operational on the computing system at the University of Tennessee (UT), where a study is being performed under a subcontract to GA. Since the computing facility at UT is an IBM system, transfer of the LAP code to the IBM system at ORNL could be quite direct. Therefore, we requested that the modification of the LAP code that exists at UT be made available to ORNL even though it is not the most recent version.

26.2 CONVERTING THE POKE CODE TO THE IBM-360 COMPUTER — D. D. Paul

The POKE code,⁴ developed by GA for the Univac-1108 computer system, was obtained from Brookhaven National Laboratory around November 1, 1974. The symbolic listing of the code was transmitted on a seven-track tape written in Binary Coded Decimal (BCD). The tape contained two files. The first file was a version of the POKE code, plus required input, for the Fort St. Vrain reactor; the second file was a version of the POKE code written for the typical 1170-MW(e) HTGR, plus the necessary input card images. The two versions of the

code were significantly different in the programming techniques involved. However, the same basic procedure was used to convert both versions to the IBM-360 computer.

The steps taken to make the POKE code operational on the IBM-360 computer were the following:

1. The seven-track tape was scanned by use of IBM-supplied utility programs for verifying the contents and determining the format used for writing the tape.

2. A simple FORTRAN program was used to rewrite the information onto a nine-track tape in a format that was more convenient for the IBM system.

3. The contents of this nine-track tape were converted from BCD to extended binary coded decimal (EBCDIC) by use of another IBM utility program. This final tape was saved for future use, and the other two tapes were erased. The erased tape that had been obtained from BNL was returned.

4. The contents of the nine-track tape were written on a private DECTAPE for use with the PDP-10 computer. The nine-track tape was stored for use in the event that part or all of the DECTAPE was accidentally lost in the editing process. The PDP-10 computer at ORNL allows maximum protection of proprietary codes in that access to the computer is through a "password" system normally used to protect the time sharing aspects of the system. Private DECTAPES can be accessed only by identified users. Card images are maintained and edited from a protected disk storage, and no hard copy is generated outside the user's control. All disk files containing the POKE code have the highest protection level; this means they are accessible only to properly identified users.

5. The FORTRAN compiler on the PDP-10 computer has been made essentially compatible with the FORTRAN compiler on the IBM-360 system. This allows the subroutines to be precompiled on the PDP-10 to eliminate all FORTRAN statements not acceptable to the IBM-360 system.

6. Proper control cards for the IBM-360 system were added to the card images, and the code was routed through a high-speed data link to that system. It was again compiled and checked for diagnostics that would arise from software and hardware differences between the IBM and Univac systems. It should be noted that many changes were required even after all the FORTRAN statements were made compatible. From the IBM-360, the printed output was routed by the system to a line printer under the user's control.

7. Editing of the code on the PDP-10 was completed using the Text Editor and Corrector (TECO) system. Many internal statements needed to be changed because of differences in system defaults, features of FORTRAN-V on the Univac-1108 not available with FORTRAN-IV on the IBM-360, and lack of the Univac system subroutines not supplied with the code for which equivalent IBM subroutines had to be added. An internal memorandum is being prepared describing the details of changes made to the POKE code. The purpose of this document is twofold. First, if any problems arise that might be traced to changes made during the conversion process, this report will be helpful in identifying the areas of interest. Second, it will provide useful information for the conversion of other GA codes to the IBM-360 system.

8. The IBM-360 version of the POKE code was run with the sample input cards. The results were compared with output generated at BNL and agreed to within 0.01%. This slight discrepancy was attributed to differences in system supplied functions, such as logarithms, exponentials, and trigonometric functions, and differences in the number of significant digits carried in a normal word for the two computing systems. The entire conversion process took about two weeks to complete. It should be noted that the conversion time was shortened considerably through the use of the PDP-10 computer and the TECO program.

26.3 RECA CODE IMPLEMENTATION — J. P. Sanders

Copies of two magnetic tapes relative to the RECA code² were obtained from Brookhaven National Laboratory (BNL) during the third quarter of FY 1975. The original tapes had been supplied by GA to BNL.

One of these tapes contained the FORTRAN source card images for the RECA code plus images of the input cards for a sample program. The other tape contained information written as an unformatted binary dump of data from the memory of the Univac-1108. This information is required input for execution of the sample problem.

The FORTRAN source listing has been modified in much the same manner as the POKE listing to be compatible with the FORTRAN compiler on the IBM 360 system. Modification of the binary information for use with the IBM 360 system presents a much greater problem. In this binary dump, the binary pattern is transferred directly from computer memory to tape. To make use of the information, the process must be reversed; then the normal functioning of the computer will use the "images" in subsequent calculations.

The first problem occurs from a difference in word length (number of binary bits assigned to store the value of a particular variable) between the Univac-1108 and the IBM-360 computer. The normal word stored in the Univac-1108 contains 36 binary bits, and the normal word length for the IBM-360 system is 32 binary bits. Therefore, a direct transfer to core in the IBM 360 of binary images created in the Univac-1108 will "overflow" the word boundaries.

Even if word boundary limits were identical, a problem would still exist because the "image format" is not the same in the two computers. The Univac-1108 uses an octal-based system to build up internal representation of values, and the IBM-360 system uses a hexadecimal-based representation. In addition, the choice of the format (the placement of the mantissa, characteristics, and sign to represent a floating-point number) also differs between the two machines.

A final difficulty is that the binary tape contains an array of integers that designate the nodal point arrangements within the system model. To save computer memory allocation, these integers were "packed" on the Univac-1108 so that multiple values are stored per word. Therefore, the packed arrays must be "restructured" in core so that an equivalent "unpacking" subroutine can retrieve the original nodal point matrix.

To circumvent this binary tape conversion, detailed inquiry was made by telephone to representatives of GA concerning the generation of this tape. This conversation led to the generation of a flow diagram for the overall execution of the RECA program as shown in Fig. 26.1. In this diagram, the two tapes that had been transmitted were the FORTRAN source card images for the transient RECA (item J), which included the required card input images (item K), and the required input binary tape (item I). If the problem of restructuring the binary tape is to be avoided so that problems other than the sample case are to be executed, it is necessary to be able to perform all operations indicated in the flow chart.

Fig. 26.1. Overall Procedure for the Execution of the RECA Program.

26.3.1 The RECA-CINDA Interrelationship

The CINDA-3G preprocessor⁵ was written under the sponsorship of the NASA program and has been in use at the ORNL computing installation for a number of years. This preprocessor requires as input the description, in a prescribed format, of nodal point information for a given interconnecting network. The output of the preprocessor is equivalent FORTRAN source statements that describe the network and the associated computation. A number of standard subroutines are available from the CINDA library and are automatically called in as required by the generated FORTRAN statements. In addition, user-supplied, special-purpose subroutines may be added to the FORTRAN statements as required.

The FORTRAN statements that are generated are usually computed directly by the local system compiler. However, the entire FORTRAN listing may be retained and used as any FORTRAN source program. It is this generated program that has been designated RECA by GA.

The immediate output of the CINDA-3G preprocessor is a version of RECA designed to solve a steady-state problem (item G). By appropriate structuring of the CINDA input, sufficient subroutines can be added so that this FORTRAN program can be converted to a version (item J) that will solve transient problems through the substitutions of a limited number of cards.

Another function of the CINDA preprocessor is to produce, for subsequent use, information concerning the connections of the nodal point network, the relative conductivities between points, and the heat capacity associated with each nodal point. At the user's option, the nodal point array may be "packed" and the total information designated to be written on tape, disk, or other convenient devices. This information is represented by the binary tape that is designated as item F in Fig. 26.1.

Execution of the compiled version of the steady-state version of RECA (item G) requires this tape (item F) plus additional information concerning system operation (item H). The output of this procedure is a second binary tape (item I) that contains the steady-state temperatures,

pressures, and flows associated with the nodal points, plus all the nodal point information read from the previous tape (item F). These temperatures, pressures, and flows represent the initial conditions for the transient calculations. Printed output (item M) is also produced.

Execution of the compiled version of the code requires the binary tape (item I) as input for initial conditions and nodal point information plus additional card input (item K) that describes the course of the transient to be followed.

26.3.2 The Nodal Point Generator, RECGEN

The CINDA preprocessor does not have a nodal point generator as a normal feature. A nodal point generator is a subprogram that converts a general physical description of the system into an equivalent representation as a series of nodal points with appropriate interconnections, conductances, and capacities. To use the processor, it is necessary to describe the system by selecting representative nodal points by hand and calculating the respective conductances and capacities and to format the information as required by the CINDA preprocessor. Since this process is repetitive for any symmetrical arrangement, it is convenient to write a computer program to perform this task. GA has written such a program; it is called RECGEN (item B); this program, if it is going to be general purpose, will require input data (item A).

Information was supplied by GA to convert the transient RECA (item J) to the steady-state RECA (item G). Execution of either requires the binary tapes (item F or I), which cannot be read directly on any system other than the Univac-1108.

26.4 EVALUATION OF THE RECA FORMULATION — D. D. Paul

The computations performed by the RECA program depend on the CINDA-3G preprocessor.⁵ Since this program was in use at ORNL, we could evaluate the formulation of the CINDA-3G input that produces the programming for RECA. In particular, the method used for calculating the conductor values between nodal points in the active core is being evaluated.

Communications have been received from GA that detail the development of the radial conductance between refueling regions. This development was reviewed together with independently developed representations by ORNL. Particular attention has been given to the relationship between the nodal point representation and the equivalent conductance between refueling regions.

The CINDA preprocessor solves the heat conduction equation in three-dimensional space where each node would have at most six neighbors. The RECA formulation represents the six refueling regions that surround each nodal point in the hexagonal arrangement in the core. By adding the two axial nodes, each core node is specified as having eight conductive paths to neighboring nodal points plus a connection to a convective coolant channel node. While the input for CINDA allows this representation, it is not obvious that the numerical techniques that are generated do not tacitly assume a limit of six orthogonal conductor neighbors per node.

Evaluation of this aspect of the code requires a review of the formulation of the CINDA preprocessor, the input structural representation created by RECGEN, and the calculation of equivalent nodal point conductances and capacitances.

Another aspect of the RECA code that was evaluated separately is the function of the subroutines added to the code by GA. The most important of these is the subroutine that determines the distribution of coolant flow among various regions of the core. An equivalent subprogram is being developed to evaluate the functioning of this feature of the RECA code.

26.5 IMPLEMENTATION OF THE BINARY DATA TAPES FOR THE RECA PROGRAM — N. E. Clapp

So that the sample problems can be executed locally, and to ensure that the adaptation of the RECA Fortran source program has been performed properly, the GA binary data tapes are being restructured so that they will be compatible with the IBM-360 system. This is being accomplished by a special bit-manipulation program called DERECA. In addition to the modification of the representation of integer and floating-point values

and alphanumeric symbols, the array of packed nodal point data is being stored in locations that are addressable as normal Fortran variables. To solve this problem, the tape format and type of data recorded on each tape record must be known. The solution to this problem will be discussed by describing the type format and the conversion of each type of data recorded.

The Univac-1108 data tape contains a number of records containing the RECA input data. The first word (36 bits) of each record is a packed control word containing three types of information. The first 18 bits of the control word contain the number of usable words in the record, the next 6 bits contain record information, and the last 12 bits contain the record count. The record information has two values, either zero or one. The one indicates that this is the last record for a given set of data, and the zero indicates it is not the last record. The record count contains the record number for a given set of data.

Following the control word are six sets of data on the tape as described in the RECA code. The first set contains alphanumeric data identifying the data set. The second set contains alphanumeric data identifying the type of test case described by the data. The third set of data contains three integer words and two floating point arrays. The fourth set of data contains the two integers and one floating point array. The fifth set contains a floating point array. The sixth set contains one integer and a packed data array.

The data records are read into the IBM-360 by use of a special program called BUFIN, which simply transfers the bit pattern from the tape into a selected section of core. This bit pattern is then decoded by the appropriate subroutine to obtain values compatible to the IBM-360. The IBM-360 is a hexadecimal byte, addressable where one byte contains eight binary bits. Then, to get the number of binary bits required for processing requires masking and shifting of bits.

26.5.1 Decoding the Control Word

The description of the decode process is presented by describing the appropriate lines in the FORTRAN program.

```

DØ    15    I=1,3

15    CALL PICKUP [KBUF, I, LC(I)] .

```

KBUF is the array name describing the storage location of the tape data. PICKUP is a special program that gets the Ith byte from KBUF and places it in the least significant byte of LC(I). Thus, LC(1) contains the first 8 bits, LC(2) contains the next 8 bits, and LC(3) contains the next 8 bits. In order to get the first 18 bits from the 24 bits stored in LC, the last six bits in LC(3) are removed by the IAND-function* using MASK1 and shifted to the right six places by:

```

L3 = IAND[LC(3), MASK1] ,

L3 = L3/64 ,

```

where

$MASK1 = (000000C0)_{16}$.

The first 8 bits stored in LC(1) are shifted to the left by eight places and added to the 8 bits stored in LC(2). Then this sum is shifted two places to the left and added to L3, which contains the first two bits of the last 8 bits:

```

NT = 4 * [256*LC(1) + LC(2)] + L3 ,

```

where NT is the number of Univac-1108 words in this record. The record information is obtained from the last six bits in LC(3) by

*The IAND-function performs a bit by bit and operation.

$$JL = IAND[LC(3), MASK2] ,$$

where

$$MASK2 = (0000003F)_{16}.$$

The record number is obtained by transferring the fourth and fifth byte of KBUF into LC(1) and LC(2) and masking out the last 4 bits of LC(2), then shifting four places to the right. Next, LC(1) is shifted 4 places to the left, and the shifted values of LC(1) and LC(2) are added as described in the following code segment:

```

DØ      16      I=4,5
        K = I-3
16 CALL PICKUP [KBUF, I, LC(K)]
        L3 = IAND[LC(2), MASK3]
        L3 = L3/16
        IB = 16*LC(1) + L3 ,

```

where

IB contains the record count, and $MASK3 = (000000F0)_{16}$.

26.5.2 Decoding the Alphanumeric Data

Alphanumeric data are converted by use of the subroutine ALNC (K,NT,NAME), where K is the byte number for start of decode, NT is the number of Univac-1108 words, and NAME is the storage array for the converted results. Because of the difference in the number of bits per word in the Univac-1108 and the IBM-360, the Univac word may start with the first bit of a byte or the fifth bit of a byte. In the Univac-1108, an alphanumeric character is described by two octal digits or six bits, and in the IBM-360, an alphanumeric character is described

by two hexadecimal characters or eight bits. The basic problem is to get the appropriate six bits defining an alphanumeric character and convert it to an eight-bit pattern representing the same character.

The Univac-1108 octal code representing the alphanumeric character list has a value range of 0 to 63. By adding 1 to this value, a range from 1 to 64 is obtained. Then by using this value as an index to an array containing the appropriate IBM-360 eight-bit code, the octal code can be converted.

Subroutine ALNC has two bit-manipulating sections. The first section gets and converts the six bits defining each of the six characters in a Univac word when the Univac word starts with the fifth bit of an IBM-360 byte. The last section gets and converts the six characters when the Univac word starts with the first byte of an IBM-360 byte. Both these sections place the converted character in the appropriate position of the storage array labeled NAME.

26.5.3 Decoding the Integer Data

Integer data are converted by use of the subroutine INTC(K,ITYPE,INCØN), where K is the byte position containing the first part of the Univac integer. ITYPE is a control word with a value of 1 if the Univac word starts with the fifth bit of an IBM byte and a value of 2 if the Univac word starts with the first bit of a byte, and INCØN is the converted integer. The only restriction in this routine is that the integer value converted cannot exceed the integer range of the IBM-360 computer. This is not a problem since no integers used in the RECA code exceed the IBM limit. Negative integers are not considered since they occur only in the packed data array, which is discussed later.

26.5.4 Decoding the Floating Point Data

Floating point data are converted by use of the subroutine FPCR (F,K,ITYPE), where F is the converted floating point value, K is the byte position containing the first part of the Univac floating point number and ITYPE is a control word with a value of 1 if the Univac word starts with the fifth bit of an IBM byte and a value of 2 if the Univac word starts with the first bit of a byte.

The Univac-1108 floating point word has the following format: The most significant bit is the sign bit, and it is 0 for positive values and 1 for negative values. The next eight bits define the characteristic. The characteristic is biased by $(200)_8$ to provide for negative exponent values. The unbiased characteristic is the exponent of 2 that defines the scale factor of the value. The least significant 27 bits describe the mantissa, or the fractional part of the number. The mantissa is the scaled fraction part of the number. Let N1 through N9 be the nine octal values representing the mantissa, then the fraction is determined by the following equation

$$\text{mantissa value} = \frac{N1}{8} + \dots + \frac{N9}{8^9}$$

To see how this works, the number $(6.0)_{10}$ will be converted into the Univac format:

$$(6.0)_{10} = .75 \times 2^3,$$

$$\text{therefore, exponent} = (3)_8 + (200)_8 = (203)_8$$

$$\text{and the mantissa} = .75 = 6/8 = (600\ 000\ 000)_8,$$

which given $(203\ 600\ 000\ 000)_8$ as the bit pattern for $(6.0)_{10}$. The bit pattern for $(-6.0)_{10}$ is simply the 1's complement of the bit pattern of $(6.0)_{10}$ or $(574\ 177\ 777\ 777)_8$.

The subroutine first checks the sign bit; if it is 1, the 1's complement is performed and then the number is converted. The conversion is accomplished in two steps. The first step determines the value of the exponent and the second step determines the value of the mantissa. These two values are combined along with the sign information to determine the floating point number in the IBM-360 format.

26.5.5 Decoding Packed Data

Packed data are converted in the main program. The packed data are written on the data tape by use of 27 records. The first record contains an integer as the first value after the control word that indicates the number of Univac-1108 words used to store the packed data. This value is decoded by using the integer-converting subroutine INTC.

A packed data word contains 27 bits and the Univac-1108 contains 36 bits; therefore, four packed data words can be placed into three Univac-1108 words. The following logical sequence is used to decode a series of eight packed data words. The data from a record is moved into core by using the BUFIN command. The first three bytes, or 24 bits, of the packed data are placed into a core location and shifted three places to the left. The fourth byte of the packed data is placed into a core location and shifted five places to the right, thus leaving the most significant three bits right justified. These two locations are then added to obtain the first packed word. The second packed word is obtained by placing three bytes into a core location starting with the fourth byte, and through the use of a masking technique, the most significant three bits are eliminated. The bit pattern is then shifted six places to the left.

The seventh byte is placed into a core location and shifted two places to the right. The two locations are then added to obtain the second packed word. The third word is obtained by placing four bytes starting with the seventh byte into a core location, and by use of masking techniques, eliminate the most significant six bytes and then shift the bit pattern one place to the left. The 11th byte is placed into a core location and shifted seven places to the right. The two locations are then added to obtain the third word. The next word is obtained by placing three bytes into a core location and eliminating the most significant bit and shift the bit pattern four places to the left. The 14th byte is placed into a core location and shifted four places to the right, leaving four bits. The two locations are then added to obtain the fourth word. In a similar manner, the remaining data in a record are decoded. Because of the difference in the number of bits per word between the Univac-1108 and the IBM-360 computers, there are some bits of data left in each record that have to be saved and then placed in front of the data read in from the next record. This procedure is repeated until all the packed data have been decoded.

26.6 ADDITIONAL MATERIAL SUPPLIED BY GA — J. P. Sanders

In view of these problems associated with the conversion of the binary tape, plus the implication that a binary tape would have to be supplied for each different problem that we wished to work, GA supplied ORNL with additional material identified in Fig. 26.1. This included the card images for the RECGEN program and the card images for GA's CINDA-3G preprocessor. These supplied two binary tapes representing F and I plus an indication of the card substitution necessary to convert G to J. GA also supplied the required input card images for each step.

The Fortran source program for the RECGEN program was converted and modified for the FORTRAN IV compiler on the IBM-360 operating system; it was made operational, and it produced the appropriate output cards to be used as input to the CINDA-3G preprocessor. This input was used with the CINDA-3G preprocessor that was previously available at ORNL, and diagnostic messages were produced by the preprocessor.

The RECA source programs, both the steady-state and transient versions, were converted and modified to be compatible with the IBM-360 system and the FORTRAN IV compiler. Problems still existed in utilizing the input data from the locally converted versions of the original packed binary data tapes. The "unpacking" subroutines were replaced in the RECA programs to enable execution of the programs; however, the output values generated are questionable.

The GA-supplied version of the CINDA-3G preprocessor is being converted and modified to accept the RECGEN output cards and to create an unpacked data tape for input to the RECA programs.

26.7 IMPLEMENTATION OF THE BLOOST-5 COMPUTER CODE — J. C. Cleveland

Implementation of the BLOOST-5 computer code⁶ on the IBM-360 is under way. BLOOST-5 is a coupled two-dimensional heat transfer-space-independent neutron kinetics code. Transient results obtained from BLOOST-5 compare quite well with results generated by the core heat transfer-neutron kinetics model developed at ORNL.

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