MASTER

REACTOR SAFETY RESEARCH PROGRAMS

QUARTERLY PROGRESS REPORT

JANUARY 1 - MARCH 31, 1977

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DEPARTMENT OF APPLIED SCIENCE BROOKHAVEN NATIONAL LABORATORY
UPTON, NEW YORK 11973

MASTER

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JANUARY 1 - MARCH 31, 1977

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FOREWORD

The Reactor Safety Research Programs Quarterly Progress Report describes current activities and technical progress in the programs at Brookhaven National Laboratory sponsored by the USNRC Reactor Safety Research Division. The projects reported each quarter are the following: Gas Reactor Safety Evaluation, THOR Code Development, SSC Code Development, LMFBR and LWR Safety Experiments, Fast Reactor Safety Code Validation, Technical Coordination of Structural Integrity, and Fast Reactor Safety Reliability Assessment.

This report is the second of a series of quarterly reports which combines the reports of the individual projects mentioned above. The previous report, BNL-NUREG-50624, covered the period October 1 through December 31, 1976.

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TABLE OF CONTENTS

	<u>Page</u>
FOREWORD	
I. HTGR SAFETY EVALUATION	1
Summary	1
1. Fission Product Release and Transport	4
1.1 Interaction of Fission Products with HTGR Materials	4
1.2 Cesium Transport in H451 Graphite	4
1.3 The Permeability and Retentivity of Stressed Concrete Relative to Fission Product Iodine	9
References	19
2. Materials, Chemistry, and Instrumentation	21
2.1 Materials	21
2.2 Materials Test Loop	35
2.3 Characterization and Oxidation Kinetics of PGX Graphite	41
2.4 Cesium Interaction with Materials	48
2.5 Helium Impurities Loop	48
2.6 Safety Instrumentation and Control Systems	50
Publications	52
References	52
3. Structural Evaluation	54
3.1 Core Seismic	54
3.2 Steam Generator Tube Weld Evaluation	55
References	58
4. Analytical	59
4.1 Thermal-Hydraulics During Natural Convection	59
4.2 HTGR Safety Code Library	66

	<u>Page</u>
4.3 Diffusion of Fission Products Through the Graphite After a Loss of Coolant Accident	67
4.4 Gas Dynamics in the Containment Building	74
4.5 Thermal Liner of PCRV: Flow Noise Excitation and Fatigue Failure Evaluation	78
References	81
II. LMFBR SAFETY EVALUATION	83
Summary	83
1. Fast Reactor Assessment - Accident Sequence Studies	84
1.1 Hydrodynamic Characteristics of Two-Phase Dispersal Systems	84
1.2 Dispersion and Heat Transfer in Two-Phase Boiling Systems	85
1.3 Boiling Pools with Internal Heat Generation	87
1.4 Streaming and Freezing of Core Debris in Channel Geometry	90
Publications	93
2. Technical Coordination of Structural Integrity	100
3. SSC Code Development	101
3.1 Transient Single-Phase Coolant Dynamics	101
3.2 Sodium Boiling and Fission Gas Release	111
3.3 Heat Transport System	111
3.4 Steam Generating System	120
3.5 System Integration Method	121
3.6 Code Development	123
Publications	124
References	124
4. Code Validation	125

	<u>Page</u>
4.1 Sensitivity Analysis Methods	125
4.2 Critical Experiment Analysis	126
References	131
5. FRS Reliability Assessment	132
5.1 Statement of the Problem	132
5.2 Unfeasibility of an Analytical Solution	133
5.3 Moment Matching	134
5.4 Monte Carlo Simulation	135
5.5 Taylor Series Approximation	136
References	137
III. LIGHT WATER REACTOR SAFETY	139
Summary	139
1. Light Water Reactor Thermal/Hydraulic Development Program	141
1.1 Development of Constitutive Relations	141
1.2 Design, Construction and Checkout of Experimental Equipment	145
1.3 Development of Instrumental Techniques	149
References	153
2. THOR Code Development	159
2.1 Demonstration Test Cases for Lumped-Parameter Formulations	159
2.2 Component Modeling	174
2.3 System Modeling	183
2.4 Correlations	188
2.5 Code Assembly	189
References	190

	<u>Page</u>
3. Appraisal of Analysis Development in Foreign Countries	192
3.1 The NAIAD Code	192
3.2 Other Codes	192
References	192
Appendix I Non U.S.A. LOCA Codes	193
References	198

I. HTGR SAFETY EVALUATION

SUMMARY

Construction of the thermochromatographic system to study the chemical state and distribution of simulated iodine, cesium, and strontium base fission products has been completed. A run has been made on a sample of CsI doped with ^{137}Cs and ^{131}I . The data are presently being analyzed.

Cesium release experiments were performed on an additional group of H451 graphite samples doped with either CsNO_3 or CsI. The results are in accord with our previous data which indicated that CsI is released from graphite in molecular form by vaporization from pores whereas elemental Cs from CsNO_3 is transported through graphite by diffusion.

The January through March 1977 work on the project "Permeability and Retentivity of Stressed Concrete Relative to Fission Product Iodine" has principally involved the completion of Sorption Experiment 3 which began last quarter. This was followed by a study of the permeation of iodine through a cube with a closed-through crack. Then a sectioning experiment was carried out to determine the iodine concentration profile in the cube used in the permeation experiment. Other work done includes the determination of the flow characteristics of the pore structure of the concrete using an inert gas (helium) to determine the permeability coefficient as a function of average pressure of gas in the concrete cube sample.

An evaluation was made of the fatigue behavior of annealed $2\frac{1}{2}\text{Cr-1Mo}$ HTGR steam generator tubing material. High-cycle fatigue data for a test temperature were shown to be compatible with low-cycle data from the literature, but care had to be exercised to ensure that specimen geometries from study to study were the same. Tests on hourglass specimens, which are most commonly used, were found to yield less conservative fatigue lives when compared to parallel-sided samples and this should be taken into consideration in reactor component design and analysis. At 1000°F , $2\frac{1}{2}\text{Cr-1Mo}$ steel does not show an endurance limit for accumulated fatigue cycles up to about 10^8 . This indicates that under long-term high-cycle conditions the fatigue strength of steam generator tubing may be significantly degraded.

Hastelloy-X thermal barrier cover plate material was also tested under high-cycle conditions at 1400 and 1600°F . At the higher temperature, and possibly at 1400°F , the fatigue strength shows a sharp decrease with time (accumulated cycles) after an initial pseudo fatigue limit had been attained. The sharp drop in strength is postulated to be a result of metallurgical changes in the material which occur after extended times at elevated temperatures.

Six creep-test machines are currently in operation. Specimens of Incoloy-800H and Hastelloy-X are being tested in the range 1200 - 1400°F . The tests are being carried out in air to serve as control data for in-helium tests which will be initiated within a few months.

Preliminary studies have been initiated on the microstructure evaluation of graphite materials which have been oxidized in flowing high-temperature helium containing water. Optical microscopic studies show that oxidation tends to occur preferentially at small pores with the large grains. The pores appear to increase in size at higher oxidation levels.

During this quarter, construction of the Materials Test Loop (MTL), which will supply prototypic helium to mechanical testing machines, was brought to an advanced stage. The impurities injection system was built and installed into the system, the IR CO, CO₂, and CH₄ monitors and the H₂O probes were calibrated, and various controllers, meters, and switches were received and incorporated into the loop. At the end of March 1977 the construction of the MTL was approximately 75% complete.

Microscopic analyses of PGX graphite were initiated to determine the distribution of the iron impurity. Large inhomogeneities in the iron concentration were found, which may prevent development of a satisfactory oxidation rate model. In situ Mössbauer spectroscopy experiments of PGX graphite oxidation by H₂O were begun in order to elucidate the role of iron during catalysis.

The oxidation kinetics of as-received PGX graphite as followed by thermogravimetry and gas chromatography indicate the presence of a reaction zone distinct from the normal catalytic and non-catalytic processes and may be affected by the nature of the iron impurity. High catalytic rates were found to favor formation of CO₂ at the expense of CO, whereas low catalytic rates gave CO₂/CO ratios similar to those obtained for the non-catalyzed reaction.

Experiments on fission product interactions with materials were continued. Preliminary experiments involve heating purified CsI (99.9%) to yield sufficient reaction product to perform a powder X-ray diffraction analysis.

Renovation of the Helium Impurities Loop (HIL) was completed this quarter with the addition of a high-temperature valve to isolate the graphite bed from the circulating helium, and the installation of a leak-proof, non-contaminating metal bellows pump to replace the old blower. The system was leak tested under static and dynamic conditions and rendered as leak-proof as practicable. Experiments to ascertain the origins of CO, CO₂, CH₄ and H₂, which appeared inexplicably in the circulating helium, have started and will continue into the next reporting period.

The infrared monitors for the HIL have been calibrated and are operational. The interfacial components for the Varian gas chromatograph have not yet arrived necessitating manual operation for hydrogen measurement. Construction is proceeding on the mass spectrometer which is to be installed to monitor impurities in the Fort St. Vrain reactor.

The computer program to solve transient thermal-hydraulics in circulating gas systems has been completed.

The adaptation of CHAP to the BNL CDC 7600 system has started, and the adaptations of SWIRL and GENMIX-4 are completed.

Analysis of the treatment of fission product evaporation and deposition used in SORSG has continued. A discrepancy in the model has been investigated.

Analysis of the gas circulation in the containment atmosphere following depressurization of the primary system through an opening on the top of the PCRV, using CIRC has shown significant effects due to buoyancy forces and little circulation in the lower parts of the containment atmosphere.

Preliminary estimations of the fatigue lifetime of the cover plate of the thermal liner on the top face of the core upper plenum have been made using the program STREC-3. Assembly of the flow noise test facility has been completed and the measurement of the damping coefficient of a full size cover plate is in progress.

Corroborative testing of the multi-column vertical array code was undertaken with the analysis and physical testing of a single column subjected to sinusoidal excitation. The analysis method was found to correctly predict the gross response characteristics observed under test.

A program to evaluate the structural integrity of the HTGR steam generator tube welds was initiated. The program is divided into three tasks; elastic methods of analysis used in the first, inelastic time-independent methods in the second, and inelastic, time-dependent methods in the third.

1. Fission Product Release and Transport

1.1 Interaction of Fission Products with HTGR Materials (S. Aronson, I. Tang, H. Munkelwitz, R. Taylor)

Construction of the thermochromatographic system has been completed. This system will be used to study the chemical state and distribution of simulated iodine, cesium, and strontium bearing fission products in potential HTGR primary circuit environments.

Schematics of the various sections of the apparatus are shown in Figures 1.1, 1.2, and 1.3. The gas inlet system is shown in Figure 1.1. The basic carrier gas is helium which is purified by passage through molecular sieve and charcoal traps and over copper at 500°C. The helium can be mixed with hydrogen, oxygen, or other gases in all proportions by utilizing three gas proportioning units obtained from the Aalborg Instrument Company. Water vapor can be introduced into the carrier gas over a range of partial pressures by passing the gas over a mixture of anhydrous oxalic acid with oxalic acid dihydrate which is placed in a U tube in a temperature controlled water-bath.

The gradient furnace is shown in Figure 1.2. The cesium, strontium, or iodine bearing sample to be volatilized is placed in the quartz tube inside the small resistance furnace at the left of the diagram. The sample is heated and the volatilized material is carried in the helium through the temperature gradient with condensation taking place along the tube. The gradient is produced by a series of twelve temperature controllers actuated by thermocouples 1 through 12. The quantities of cesium, strontium, or iodine deposited throughout the tube are monitored with a gamma detector which measures ^{137}Cs , ^{131}I , or ^{85}Sr . The gamma detector can be manually moved along the length of the tube to monitor the distribution of condensate.

A schematic of the sample probe is shown in Figure 1.3. The sample is held in a closed graphite crucible which is contained in a quartz cup. This cup is attached to a long quartz tube in which a thermocouple is inserted. The sample can be moved forward and backward in the reaction tube by the use of a Wilson seal.

A run has been made on a sample of CsI tagged with ^{137}Cs and ^{131}I . The sample was volatilized at 800°C and was condensed along a stainless steel sleeve inserted in the quartz tube. The data obtained are presently being analyzed.

1.2 Cesium Transport in H451 Graphite (S. Aronson, J. Mittelman - Brooklyn College)

Cesium release experiments were performed on an additional group of H451 graphite samples doped with either CsI or CsNO_3 . A release experiment performed on a graphite sample soaked in aqueous CsNO_3 is shown in Figure 1.4 (Run 4-3). The initial adsorbate concentration was 0.125 mg Cs/g C. The initial count rate was 47.5 counts/sec. Release of cesium was first followed in vacuo to 583°C. Helium, at one atmosphere pressure and a flow rate of 50 cc STP/min, was introduced

Figure 1.1. Schematic of gas inlet system for thermochromatographic system.

Figure 1.2. Schematic of quartz reaction tube and gradient furnace.

Figure 1.3. Schematic of sample holder and insertion probe for thermochromatographic apparatus.

Figure 1.4. Release of cesium from CsNO_3 -doped graphite sample in vacuo, in helium, and in helium with water vapor.

and the sample was heated to 654°C. Water vapor at 9 torr was then added to the helium stream. The data in Figure 1.4 indicate that the rate of cesium release is not dependent on whether the environment is vacuum, helium, or water vapor. These results confirm and extend our previously reported data (Aronson, 1976) on the effect of vacuum and helium on cesium release from CsNO₃-doped graphite.

The results of a release experiment performed on a CsI-doped graphite sample are shown in Figure 1.5 (Run 4-6). The initial adsorbate concentration was 0.142 mg Cs/g C with an initial count rate of 48 counts/sec. Release of cesium was first followed in vacuo to 380°C. Helium was introduced and the sample was heated to 478°C. After 50 minutes at that temperature, the system was re-evacuated and the release was followed for one hour. Upon evacuation, the temperature dropped from 478°C to 455°C where it remained until the end of the run. The most significant experimental observation is the large increase in release rate which accompanied the re-evacuation at 478°C. This result is in agreement with our previously reported data (Aronson, 1976) on the comparison of release rates from CsI in vacuo and in helium.

The above experimental results and the results of additional runs in this group of experiments confirm our previous conclusion (Aronson, 1976) that CsI is released from graphite in molecular form by vaporization from internal pores whereas elemental cesium (from CsNO₃) is transported through the graphite by a diffusion process.

Samples of CsOH have been irradiated at BNL. These will be used in release experiments with CsOH or with a mixture of CsOH and Sr(OH)₂.

1.3 The Permeability and Retentivity of Stressed Concrete Relative to Fission Product Iodine (L. R. Zumwalt, J. F. Mirza - North Carolina State University)

1.3.1 Sorption Experiments

Iodine Sorption Experiment 3 was completed in January. As was mentioned in the October-December Quarterly Report, equilibrium condition for $P_{I_2} = 3.95 \times 10^{-5}$ atm. was obtained. The saturation value at this iodine vapor pressure was 711 µg/g concrete. Desorption was begun with $P_{I_2} = 2.82 \times 10^{-6}$ atm. Before equilibrium condition could be reached, a problem developed in the counting system electronics. The experiment was terminated because of this fact. The count rate due to iodine (proportional to concentration) as a function of time is given in the October-December 1976 Quarterly Report.

The value for the concentration of iodine at equilibrium condition at $P_{I_2} = 3.95 \times 10^{-5}$ atm. is higher than the value obtained in Sorption Experiment 2 (489 µg I₂/g concrete). This may be due to a difference in structure (i.e. internal surface area) of the concrete. BET surface area measurement using krypton sorption technique, done by Micromeritics Instrument Corporation resulted in a surface area of 3.62 m²/g concrete. This was appreciably larger than the BET surface area measurement by nitrogen adsorption done by Coors Spectro-Chemical Laboratory on another sample. A value of 2.9 m²/g concrete was obtained by Coors. Additional sorption work will be done in conjunction with sectioning work to be done next quarter.

Figure 1.5. Release of cesium from CsI-doped graphite sample in vacuo, and in helium.

1.3.2 Permeability Experiments

The major work done during the quarter was a permeability experiment with the through crack closed by approximately 460 psi applied pressure. In this experiment, helium gas is used as a carrier to transport iodine past one face of the concrete cube. Helium sweep gas flows past the opposite cube to remove any tagged iodine that permeates the concrete sample and transport it to an activated carbon trap (see Figure 1.6). The concrete cube and activated carbon trap were monitored for content of tagged iodine. No detectable amount of iodine was found to permeate through the concrete. The corrected count rate due to iodine uptake by the concrete as a function of time is given in Figures 1.7A through 1.7D. The iodine was supplied to the concrete face at a faster rate than the rate of uptake of the concrete. As can be seen in Figure 1.7A, there is a fast initial rise in the count rate that very quickly changes to a constant rate of uptake of iodine. The fast initial rise is possibly caused by strong absorption on the external surface. As this surface is covered, the rate of uptake of iodine is then controlled by the transport of iodine into the internal structure. Sections were collected from different depths to determine iodine concentration at each depth and to find the depth the iodine had penetrated. The results of this work will be given in a later section of this report.

1.3.3 Inert Gas Permeability

Work has been carried out using helium gas to determine the permeability coefficient $K(\bar{P})$ at different average pressures ($\bar{P}$). The apparatus used for these experiments is shown in Figure 1.8. The data obtained from this apparatus (flow rate, ΔP , and average pressure) was analyzed according to the theory of flow through porous media given in the last Quarterly Report.

The permeability coefficient as a function of average pressure of the gas is given in Figure 1.9. A nonweighted least squares fit of this data yielded values of $K(0) = 0.3478 \text{ cm}^2/\text{sec}$ and a slope of $4.899 \times 10^{-7} (\text{cm}^2/\text{sec}) \text{ per } (\text{dyne}/\text{cm}^2)$. From this the effective pore radius (r_p) was determined to be $1.0969 \text{ } \mu\text{m}$, and the ratio of the effective porosity to tortuosity squared (ϵ'/q^2) was determined to be 0.0646. The r_p is of a larger value than the average pore radius as determined by the mercury penetration method. This method of measuring average pore radius, assuming a contact angle of 130° , was done by Micromeritics, and the average pore radius was determined to be $0.12 \text{ } \mu\text{m}$. One would expect the effective pore radius to be larger because it accounts only for the large through pores involved in the flow process. As was stated in the last Quarterly Report, r_p and ϵ'/q^2 will be used to determine the effective in-pore gas phase diffusion coefficient of iodine in concrete.

1.3.4 Sectioning Experiment

The concrete cube used in the iodine permeation experiment was sectioned to obtain a concentration profile. A drill press utilizing a solid carbide fluted spiral end mill bit (flat-end) was used to drill a core out of the cube in very thin sections. After each small section was drilled out, the concrete dust was collected on a filter paper using a suction device. The samples from

Figure 1.6. Iodine permeability experimental apparatus.

Figure 1.7A. Count rate due to iodine sorption in concrete sample - period 0 to 16,000 minutes - Permeability Experiment 2.

Figure 1.7B. Count rate due to iodine sorption in concrete sample - period 16,000 to 32,000 minutes - Permeability Experiment 2.

Figure 1.7C. Count rate due to iodine sorption in concrete sample - period 32,000 to 48,000 minutes - Permeability Experiment 2.

Figure 1.7D. Count rate due to iodine sorption in concrete sample - period 48,000 to 64,000 minutes - Permeability Experiment 2.

Figure 1.8. Inert gas permeability apparatus.

Figure 1.9. Permeability coefficient of helium in concrete as a function of average pressure on concrete cube.

different depths were counted in a calibrated NaI(Tl) well-type detector and weighed to yield a concentration profile. The concentration profile is given in Figure 1.10.

The iodine penetrated less than one inch into the 3-inch cube. From the shape of the curve and the limited penetration, we concluded that there must be strong absorption of iodine by the concrete. The cube face on the source side was exposed to a large excess of iodine with $P_{I_2} = 3.95 \times 10^{-5}$ atm. for 44 days. One would expect simple gas phase in-pore diffusion to have penetrated the cube in much less time than this. In the next quarter we shall do more time dependent sectioning to see if we may fit the solutions of the following diffusion-sorption model equations.

$$\frac{\delta C_p}{\delta t} = D_p \nabla^2 C_p - \frac{\delta C_b}{\delta t} \quad (1.1)$$

$$\frac{\delta C_b}{\delta t} = k_1 C_p - k_2 C_b \quad (1.2)$$

where

C_p = Sum of the concentration of iodine in the gas-phase plus the surface phase of the large pores (per unit bulk volume) - a function of spatial coordinates and time.

C_b = Concentration of iodine in the small pores (per unit bulk volume) - a function of the coordinates and time.

D_p = Effective in-pore diffusion coefficient - for combined pore surface and in-pore gas phase diffusion.

k_1 = Rate constant for transfer of molecule from large pore surfaces to small pores of the concrete (see $^{-1}$).

k_2 = Rate constant for the reverse process (see $^{-1}$).

It is to be noted that the extrapolated boundary concentration in Figure 1.10 of 910 $\mu\text{g I}_2/\text{g concrete}$, should correspond to a true saturated sorption at $P_{I_2} = 3.95 \times 10^{-5}$ atm. and 25°C. This is of the same order but somewhat higher than the value of 711 $\mu\text{g I}_2/\text{g concrete}$ reported in February 1977 for Sorption Experiment 3. The first two points on Figure 1.10 are believed to be low because iodine had time to diffuse from the near surface region of the concrete cube before sectioning was begun.

REFERENCES

ARONSON, S. and MITTELMAN, J., "Cesium Transport in H451 Graphite," in Reactor Safety Research Programs Quarterly Progress Report, October 1-December 31, 1976, Brookhaven National Laboratory, BNL-NUREG-50624 (February 1977), p. 5.

Figure 1.10. The iodine concentration profile for cube exposed on one face to $P_{I_2} = 3.95 \times 10^{-5}$ atm. for 44 days.

2. Materials, Chemistry, and Instrumentation

2.1 Materials (J. Chow, P. Soo, J. E. Zimmer)

This program covers the evaluation of four main categories of materials: (1) metallic materials, (2) graphites, (3) PCRV, and (4) other materials. Included in (4) are control rod materials and thermal barrier insulation. At this time the major emphasis is centered on metals. The objectives of the work are to critically review available materials data pertinent to HTGR safety, identify areas where information is sparse or unavailable, and to design and initiate experiments to yield data which will permit accurate assessments to be made of materials-related safety problems.

A Materials Test Loop (MTL) is being constructed for supplying prototypic HTGR helium environments to selected creep and fatigue test machines and also for thermally aging specimens prior to creep and fatigue testing.

Subcontracted work at the Aerospace Corporation is concerned with the development of an analytical model which will predict the fracture strength and fracture mechanisms of oxidized graphites.

2.1.1 Mechanical Properties of Materials

The new 55,000 pound capacity MTS fatigue unit has been fully commissioned and is operating routinely. Five fatigue machines are currently being used in the program; all have capability for high-cycle testing and two are being adapted for future low-cycle work.

During this report period eight Applied Test Systems lever-arm creep test units have been set up, aligned, calibrated, and are now in operation. These test units are all equipped with environmental retorts for testing in helium with controlled impurities. The helium environment for these units will be supplied from the MTL loop. This is still under construction and creep testing is currently being conducted in an air environment to establish baseline data from control samples.

(a) Fatigue Testing of Annealed $2\frac{1}{2}\text{Cr-1Mo}$ Steel

An evaluation is being carried out of the structural integrity of HTGR steam generator tubing. In this phase of the work annealed $2\frac{1}{2}\text{Cr-1Mo}$ steel is being studied for high-cycle fatigue conditions to determine the effects of high-frequency loading such as would be expected from flow-induced vibration and rapid thermal fluctuations. The material being studied is an annealed pipe supplied by the Metals Property Council, New York. Table 2.1 gives the chemistry. It was annealed at 1700°F for 2 hours, furnace cooled at 50°F/h to 1100°F, and then air cooled to room temperature. Specimens of hourglass geometry were machined from the heat-treated pipe and left in the surface-ground condition.

TABLE 2.1

Chemistry of Annealed 2½Cr-1Mo Steel Pipe

<u>Element</u>	<u>Concentration (wt. percent)</u>
C	0.145
Mn	0.40
Cr	2.2
Mo	0.94
Ni	0.30
S	0.019
P	0.014

Testing was carried out on MTS closed-loop electrohydraulic machines under load control. A sinusoidal load waveform was used at a cycling rate of 40 Hz. All testing to date is at 1000°F. Table 2.2 gives the data obtained both in this program and in a previous one (Chow, 1972; Chow, 1973).

As discussed in prior reports (Soo, 1976; Soo, 1977), high-cycle and low-cycle fatigue test procedures are basically different since the former involves load control at low stress levels, whereas the latter is concerned with strain control at higher stresses. The major differences lie in the way steady-state, stress-strain conditions are achieved, and in the large differences in the cycling rates. In the earlier studies it was shown that high- and low-cycle data for Incoloy-800H and Type 304 stainless steel were correlatable as functions of both the applied stress, σ , and total strain range, $\Delta\epsilon_t$, if the following criteria are met:

- For the σ - N_f data the effective stresses for the low-cycle data are taken to be the values at half the fatigue life, i.e. at $N_f/2$.

- For the $\Delta\epsilon_t$ - N_f curves the effective strains for the high-cycle tests are obtained from cyclic stress-strain curves, which relate the stable stress and stable strain levels during test.

The above guidelines were used here in an attempt to correlate the high-cycle data for annealed 2½Cr-1Mo and available low-cycle data from the literature (Jaske, 1970; Brinkman, 1975). Brinkman's study contains a compilation of information from several sources including Battelle Memorial Institute (BMI), General Atomic (GA), and Mar Test Laboratory. These are acknowledged in the figures of this report. Most of the low-cycle data considered are for a strain rate of $4 \times 10^{-3} \text{ sec}^{-1}$, since this was most common.

Figure 2.1 shows the correlation with the low-cycle data as a function of the alternating stress. The stress values for the low-cycle data were those cited by the various experimentalists, and represent the values at $N_f/2$. Comparisons with data other than those from ORNL and BMI were not possible since the stress levels were not given. A good correlation between the ORNL and BNL

TABLE 2.2

High-Cycle Fatigue Data for Fully Annealed

2½Cr-1Mo Steel Pipe at 1000°F⁽¹⁾

Specimen No.	Location in Pipe ⁽²⁾	Alternating Stress (ksi)	Cycles to Failure ⁽³⁾
MC1-70	MW	42.0	4.8×10^3
MC1-75	MW	38.0	4.8×10^3
MC1-78	ID	32.0	7.4×10^3
MC1-66	OD	32.0	9.9×10^3
MC1-64	OD	28.0	3.5×10^6
MC1-62	OD	25.0	1.0×10^7
MC1-79	ID	25.0	1.1×10^6
MC1-65	OD	22.0	7.1×10^7
MC1-80	ID	22.0	2.9×10^7
MC1-76	MW	20.0	6.1×10^7
MC1-82	ID	20.0	1.5×10^7
MC1-63	OD	20.0	$>2.0 \times 10^8$

- NOTES: (1) The pipe was 10" OD x 1.972" wall. It was heated for 2 hours at 1700°F, furnace cooled at 50°F/h to 1100°F and then air cooled to room temperature. The average carbon content is 0.145%.
- (2) Samples were cut along the axial direction of the pipe and were from the ID, OD, or the mid-wall (MW) locations.
- (3) Cycling rate is 40 Hz.

HIGH-CYCLE FATIGUE OF ANNEALED 2-1/4 Cr-1 Mo STEEL
 AT 1000°F AS A FUNCTION OF STRESS

Figure 2.1

results was obtained although some scatter was evident in the high-cycle data. However, there was a major discrepancy with the BMI data which gave fatigue lives about two orders of magnitude lower than those obtained by ORNL and BNL. Possible reasons for this anomalous behavior are:

- . Surface finish
- . Strain rate
- . Material chemistry
- . Size effects and geometry.

The BNL samples were given a machine-ground finish whereas the BMI samples were given a final hand finish through 600-grit emery paper. Since these are similar finishes the wide difference in N_f values is probably not due to surface effects. Strain rate effects are also unlikely to be responsible since the respective strain rates for the ORNL and BMI tests were 4×10^{-3} and 1.7×10^{-4} sec^{-1} , i.e. they differ by about an order of magnitude. From the data given in Figure 2.2 (Brinkman, 1975) it is clear that in the high-cycle fatigue range strain rate effects are not sufficiently large to account for the 100 times difference in fatigue life between the ORNL and BMI tests. Material chemistry is also unlikely to explain the lower fatigue strengths in the BMI tests since the BNL and BMI results were obtained from the same pipe material.

It appears that the most plausible explanation is connected with specimen geometry. Both the ORNL and BNL samples had an hourglass-shaped gauge length, whereas the BMI samples have a straight-sided one-inch gauge length. Studies have demonstrated that a large effective surface area increases the probability of crack initiation, and this would help explain the anomalous BMI results (Yokobori, 1964). Specimen geometry must, therefore, be considered an important variable in determining fatigue life in reactor structural steels since parallel-sided samples appear to yield more conservative data when compared to the more commonly used hourglass type.

An important observation in Figure 2.1 is that there is not a well-defined fatigue limit at 1000°F in annealed 2½Cr-1Mo steel. This indicates that fatigue failure may present a problem, even at relatively low stress levels. Such behavior is in contrast to Incoloy-800H and annealed Type 304 stainless steel (Soo, 1976; Soo, 1977) which show stable fatigue strengths for N_f values in excess of about 10^5 cycles (see Figure 2.3). The lower fatigue strengths of 2½Cr-1Mo will be magnified if any in-service decarburization occurs since decreased carbon levels will lead to a general weakening of the steel.

The data in Figure 2.1 also show a dependence of fatigue strength on the location from which the specimens were machined from the pipe. Samples cut from regions adjacent to the outside surface were generally stronger, especially for the very high-cycle range. Samples cut from the mid-wall region were intermediate in strength, and internal diameter samples were the weakest. This observation is compatible with chemical analyses conducted by Jaske (1970) on the same pipe material. He showed that for two specimens cut from the outer pipe surface the carbon levels were 0.17 and 0.15%, whereas for two samples from the mid-wall region

EFFECT OF CYCLING RATE ON THE HIGH-CYCLE FATIGUE OF
ANNEALED 2-1/4 Cr-1 Mo STEEL AT 800 °F

Figure 2.2

COMPARISON OF THE 1000° F FATIGUE STRENGTHS
OF ANNEALED 2-1/4 Cr-1 Mo STEEL, TYPE 304
STAINLESS STEEL AND INCOLOY-800 H

Figure 2.3

the concentrations were both 0.13%. It is apparent that significant changes in high-cycle fatigue strength may result from differences in carbon concentration brought about by processing variables. Care must be taken to account for such deleterious effects in design.

In order to correlate the high- and low-cycle data as a function of strain range, available cyclic stress-strain curves from the literature have been plotted in Figure 2.4. The cyclic stress-strain curve obtained from the ORNL hourglass sample shows a significantly higher strength than parallel-sided BMI samples. Similar behavior has been previously observed from monotonically-loaded austenitic materials (Conway, 1975) indicating that this is a general geometrical effect. Hence, care must be taken to ensure that the cyclic stress-strain curve used for stress-to-strain conversion are for the relevant geometry. If, for example, the BNL high-cycle fatigue stresses were converted to equivalent strains using the BMI instead of the ORNL curve in Figure 2.4, a larger effective strain range would be obtained, and give much less conservative fatigue curves.

The fatigue data in Figure 2.5 were, accordingly, obtained using the ORNL curve in Figure 2.4. A good correlation is obtained between the BNL and ORNL hourglass samples, although some scatter exists in the BNL results at the higher strain levels. This effect could be associated with internal heating which was observed in a few specimens during the very early stages of deformation.

(b) Fatigue Testing of Hastelloy-X

High-cycle fatigue testing continued on Hastelloy-X in this reporting period. Testing has been completed for 1400°F and a new series of tests was initiated at 1600°F. Table 2.3 tabulates all data generated to date, and Figure 2.6 shows a correlation with available low-cycle data from the literature (Strizak, 1976). For both test temperatures the low- and high-cycle data are compatible in the range where they overlap, indicating that the differences in cycling rate are not significant.

An important observation is the fact that for long test times (high N_f) the fatigue strength begins to show a decrease, especially for the higher test temperature. It seems that some type of metallurgical change has occurred causing a sharp decrease in fatigue resistance. The concept of a well-defined fatigue limit clearly does not apply to Hastelloy-X at elevated temperatures; hence, its resistance to fatigue damage after prolonged exposure to high temperatures in the HTGR thermal barrier cover plates must be thoroughly evaluated and quantified.

(c) Stress-Rupture and Creep Testing

Review and assessment of HTGR structural metals used in the primary circuit have shown that the long-term effects of impure helium environments on the stress-rupture and creep properties of these alloys have not been adequately evaluated. The review indicates that environment could have a significant effect on the integrity of the structural metals at the HTGR operating temperatures and

CYCLIC STRESS & STRAIN CURVES FOR ANNEALED
2-1/4 Cr-1 Mo STEEL AT 1000° F

Figure 2.4

HIGH-CYCLE FATIGUE OF ANNEALED 2-1/4 Cr-1 Mo STEEL
AT 1000 °F AS A FUNCTION OF STRAIN

Figure 2.5

TABLE 2.3

High-Cycle Fatigue Data for Hastelloy-X

Specimen	Test Temp. (°F)	Alternating Stress (ksi)	Cycles to Failure (N_f) ⁽¹⁾
MHF-13	1400	40.0	1.3×10^4
MHF-14	1400	38.0	2.1×10^4 ⁽²⁾
MHF-15	1400	36.0	3.0×10^4
MHF-19	1400	36.0	1.4×10^6
MHF-12	1400	35.0	1.2×10^7 ⁽³⁾
MHF-17	1400	35.0	1.3×10^7
MHF-16	1400	34.0	5.5×10^6
MHF-10	1400	30.0	1.8×10^7
MHF-11	1400	28.0	$>1.7 \times 10^8$
MHF-9	1400	25.0	$>7.3 \times 10^7$
MHF-24	1600	29.0	2.2×10^4
MHF-23	1600	28.0	3.6×10^5
MHF-22	1600	26.0	1.2×10^6
MHF-25	1600	25.0	7.8×10^5
MHF-26	1600	23.0	7.7×10^6
MHF-27	1600	21.0	1.7×10^7
MHF-28	1600	18.0	6.9×10^7 ⁽⁴⁾

- NOTES: (1) Cycling rate is 40 Hz.
 (2) Sample failed close to thermocouple spot weld.
 (3) Estimated value - counter continued to run after failure.
 (4) Cycle accumulation on 3/21/77.

CORRELATION OF HIGH- AND LOW-CYCLE FATIGUE
DATA FOR HASTELLOY-X AT 1400 AND 1600 °F

Figure 2.6

that more data are required for safety-evaluation studies of components made of Incoloy-800H and 2½Cr-1Mo steel which are used in the steam generator, and Hastelloy-X which is used for the cover plates of the thermal barrier. Eight creep units, equipped with retorts for testing in helium with controlled amounts of impurities, have been set up to evaluate the integrity of these alloys under HTGR operating conditions.

For Incoloy-800H, tests are in progress at 1200°F for stress levels of 20.0, 22.0, 25.0, and 26.0 ksi. Two tests on Hastelloy-X are being conducted at 1400°F at stress levels of 16.0 and 18.0 ksi. Failure times between 1000 and 3000 hours are anticipated. Upon completion of these tests the MTL helium loop should be in operation and in-helium testing will commence.

(d) Microstructure and Fracture of HTGR Graphites

The behavior of graphitic materials in HTGR's is dependent on their unique microstructures. This program is directed toward the study of the relation between microstructure and the fracture of graphites exposed to various reactor environments. This information will be used to provide improved bases for the evaluation of reactor safety.

Tension and compression specimens that have been oxidized in the BNL HIL and tested are to be observed with the use of optical and scanning electron microscopy. This analysis is aimed at relating the fracture surface features to the level of oxidation burn-off and changes in the graphite microstructure. PGX graphite specimens were exposed in the HIL to helium containing 100 ppmv water at temperatures to 750°C and times to 1974 hours. The fractured tensile specimens were sectioned radially (Figure 2.7) and one half polished to observe the trace of the fracture edge and the underlying microstructure. The fracture surface of the other half specimen is to be observed by scanning electron microscopy. A mounting medium that may obscure some microstructural features was not used. Optical micrographs were taken with partially crossed polarized light to give the microstructure some shading representing the orientation of small regions of preferred orientation.

Representative specimens of PGX graphite (Table 2.4) tested in tension have

TABLE 2.4

Burn-off Level and Tensile Strength of PGX Graphite Specimens

<u>Specimen Number</u>	<u>Weight loss, percent</u>	<u>Direction</u>	<u>Tensile Strength, psi</u>
G-17	0	AG	1235
G-15	0	AG	540
1-3-C	2.08	AG	995
1-4-G	10.21	AG	656
G-6	0	WG	1010
G-11	0	WG	1025
1-4-B	0.97	WG	906
3-3-E	2.88	WG	815
G-34	6.47	WG	565

Figure 2.7

been observed with low-magnification optical microscopy. A montage of the entire width of the specimens has been prepared. The micrographs of the against-grain tensile specimens are presented here; the micrographs of the with-grain specimens showed similar response.

The fracture edge of an unoxidized tensile specimen is shown in Figure 2.8. The fracture appears to have followed the boundaries of the large grains or grain agglomerates. These grains appear as roundish regions sometimes outlined by large pores. This fracture behavior may be simply described as intergranular.

For two oxidized tensile specimens at burn-off levels of 2.08 and 10.21 w/o, the micrographs in Figures 2.9 and 2.10 show that the oxidation tends to occur preferentially at the small pores within the large grains. The large pores are not appreciably enlarged. At the lower burn-off, the pores are small and more numerous than in the unoxidized specimen. At the higher burn-off, these pores appear somewhat larger. The fracture edge of the oxidized specimens appears not to follow the large-grain boundaries but to follow a more random path through the fine pore structure. This fracture behavior may be simply described as intra-granular. More work will be done to explicitly define the detailed fracture behavior and its relation to the graphite microstructure.

One of five unoxidized tensile specimens in the against-grain direction (G-15) had a strength that was one half the average. This specimen was observed microscopically to determine the cause of this anomalous behavior. Figure 2.11 shows that this region of the tensile specimen had very little binder present. The large grains are joined at only small areas with some grains appearing to be totally unbonded. This lack of bonding and cohesiveness produced the low strength value.

2.2 Materials Test Loop (L. Epel)

The Materials Test Loop (MTL) construction continued during the first quarter of CY 1977 at the scheduled total effort of 3 man-months. The loop is now approximately 75% completed and should be finished during the coming quarter.

The temperature controllers for the purification system were received and installed. The various tanks and vessels have been charged with reactants and all that remains to be done on this system is the installation of thermal insulation. The infrared monitors and the H₂O probes have been calibrated. The impurities injection system has been completely built, installed, and leak tested during this quarter.

Figure 2.12 is a photograph of the MTL taken during the middle of this reporting period. In the lower left of the photograph the MB601-HT metal bellows pump and motor are visible. In the background, most of the purification system can be seen, with the heater wires wound around the reactant tanks. Behind the technician the rear assemblies of the infrared monitors are apparent in the cabinets. These cabinets also house the moisture meters, the data acquisition and alarm system, the temperature controllers and variacs, and numerous other switches, meters, and controls.

Figure 2.8 Optical micrograph of fracture edge of PGX graphite tensile specimen G-17. against grain, unoxidized.

Figure 2.9 Optical micrograph of fracture edge of PGX graphite tensile specimen 1-3-C, against grain, 2.08 w/o burnoff

Figure 2.10 Optical micrograph of fracture edge of PGX graphite
tensile specimen 1-4-G, against grain, 10.21 w/o burnoff

Figure 2.11 Optical micrograph of fracture edge of PGX graphite tensile specimen G-15, against grain, unoxidized

Materials Test Loop Showing Purification System

Figure 2.12

2.3 Characterization and Oxidation Kinetics of PGX Graphite (F. Growcock, D. Chandra, J. Skalyo, Jr.)

2.3.1 Mössbauer Spectroscopy

The program to characterize the nature of the iron impurity in PGX graphite has continued. The use of Mössbauer spectroscopy has aided greatly in determining the chemical environment of the iron in PGX graphite. Indeed, the iron is found to be in clusters of relatively pure iron with a minimum size of $\sim 120 \text{ \AA}$ (Chandra, 1977). The iron is also easily oxidized or reduced, and an investigation has determined that the clusters take the form of metallic iron, Fe_2O_3 , Fe_3O_4 , FeO , and probably an austenitic phase. The latter is observed after the PGX has been subjected to a reducing environment and amounts to about 10% of the total amount of iron. The Mössbauer technique differentiates the various forms of iron mainly by sensing a difference in their magnetic properties. The type of iron species present in PGX will determine the catalytic enhancement of graphite oxidation under various HTGR primary coolant environments.

Further characterization of the nature of the iron will be attempted with the initiation of in situ equilibrium Mössbauer measurements on PGX at various temperatures and in various environments. A furnace has been built and measurements have been initiated. Substantial beam attenuation of the 14.4 keV gamma-ray source has been encountered, but the method should be applicable to PGX graphites with large amounts of iron impurity ($>1500 \text{ ppm}$, probably normal for typical PGX). A typical experimental measurement will necessarily take about a day.

Initial measurements were made on a pellet of BN loaded with reduced iron powder ($\sim 15 \text{ mg Fe/cm}^3$). The results showed a paramagnetic environment at 500°C . This is well below the bulk magnetic transition temperature and is evidence of the known dependence of the transition temperature on particle size. Measurements on PGX graphite have been initiated.

The Mössbauer measurements probe a macroscopic portion of a PGX sample ($\sim 0.5\text{--}2 \text{ cm}^2$). An investigation has been initiated to determine the microscopic distribution of iron in PGX; an upper limit to the size of the particles cannot be obtained from Mössbauer spectroscopy data. A scanning electron microscope (SEM) was used to obtain a magnified (2000X) view of the surface of a graphite sample and an X-ray emission (XES) picture was taken of approximately the same area with the instrument set to show the location of iron. The results are shown in Figures 2.13 and 2.14, respectively. An XES picture is shown of approximately the same area in Figure 2.15 for 5000X magnification. The sample had been previously shown by Mössbauer spectroscopy to have 1100 ppm iron. No special treatment was made of the sample surface, and a correlation of the surface features of Figure 2.13 to the iron density in Figure 2.14 is not evident. Evidence is shown in Figures 2.14 and 2.15 of the wide variation of iron density on a microscopic scale.

The measured oxidation rate of a macroscopic sample may be merely a determination of some complex average; the catalytic effect of the iron may not correlate

Scanning Electron Micrograph of a Surface of PGX Graphite (Area Shown is $\sim 42 \times 36 \mu$)

Figure 2.13

Iron Characteristic X-Ray Emission Spectroscopy Raster Scan of PCX Graphite Surface
(Area Shown is $442 \times 36 \mu$)

Figure 2.14

Iron Characteristic X-Ray Emission Spectroscopy Raster Scan of PGX Graphite Surface
(Area Shown is $\sim 17 \times 15 \mu$)

Figure 2.15

simply with its macroscopically measured concentration. The measurement of the microscopic distribution of iron in PGX will continue; we are currently investigating the availability of an SEM with greater magnification capability.

2.3.2 Oxidation Kinetics of PGX Graphite

The Cahn R-1000 electrobalance previously purchased was slightly modified to accommodate pressures up to ~ 4 psig, the maximum expected pressure to be applied under high flow rate conditions. Design calculations indicate that unrealistically large flow rates and system tubing would be required for the measurement of pseudo-first order rates (for $<2\%$ decrease in H_2O concentration) of the catalyzed oxidation of PGX graphite. A compromise in the design was effected which will provide pseudo-first order rate values for the uncatalyzed oxidation of PGX graphite by H_2O , as well as provide adequate approximations of the expected high rates obtained for the catalyzed reaction. Two $1/16$ " stainless steel lines leading from the hang-down flow tube to a custom-built Carle gas chromatograph Model 311M ultimately lead to a Cole-Parmer Masterflex tubing pump, which forcibly draws a small portion of the reactant and product gases through the chromatograph sample inlet. Temperature profiles of the flowing gas in the balance hang-down tube have been measured at various nominal temperatures for calibration purposes, as well as to determine the plateau region within which the sample is to be placed; this constant temperature region ($\pm 2^\circ C$) was found to be only ~ 4 cm long, but is considered adequate. The PGX graphite samples to be examined by thermogravimetric analysis have been standardized to be $1/4$ " or $1/2$ " thick tapered discs of a maximum diameter which gives an optimum signal/noise ratio and allows the samples to be used in the Flow-through System described in the previous quarterly progress report. A schematic of the Thermogravimetric and Flow-through Systems is given in Figure 2.16.

Initial thermogravimetric measurements were made at constant temperature on as-received PGX graphite specimens containing >1000 ppm iron (as measured by Mössbauer spectrometry). It was learned that the oxidation of these samples in a He/H_2O stream exhibits three time-dependent weight-change regimes, as illustrated in Figure 2.17: Region I is marked by rapid weight loss, Region II by a mild weight gain and Region III by a mild weight loss. Gas chromatographic analyses of the product gas mixtures indicate that in Region I a large amount of H_2 is formed; $[CO_2]/[CO]$ varies considerably, depending on the catalytic rate: at high rates the ratio may approach a value of 10^3 , whereas at low rates it may be $\ll 1$. In addition, at high rates $[H_2O]$ is depleted to such an extent that apparent mass transfer control of the rate is observed; reactant starvation is actually the cause, which will inevitably lead to invalid rate parameters. In Region II the rate is considerably lower, of course, and is indistinguishable from that of Region III; here the H_2 concentration is below the detectability of the instrument, $[CO_2]/[CO] \ll 1$, a trace amount of CH_4 is observed, and $[H_2O]$ is virtually identical to $[H_2O]_0$, the initial concentration of H_2O .

The trends observed for similar experiments in the previously-mentioned flow-through system are consistent with the above. Grafoil was found to be unsuitable as a sealer for the graphite in this system, since various highly aromatic compounds,

Figure 2.17. Typical thermogram of the oxidation of an as-received PGX graphite sample by He/1.0% H₂O at 16 psia and 750°C.

apparently in the adhesive backing, are given off during heating of the sample. Most of the measurements in the flow-through system were performed without a sealant; the tapered ground quartz of the reactor tube is expected to form an adequate seal with the tapered ground wall of the graphite sample at the low pressure differentials used for the highly permeable PGX graphite.

Some hydrogenation/oxidation experiments were also begun. Hydrogenation with an 8.5% H_2 in He mixture of a PGX sample oxidized to Region III proceeds with a weight loss rate not very different in absolute value from the weight gain observed in Region II of the H_2O oxidation treatment, but the integrated weight loss is many times greater than the weight gain. Large amounts of CO are produced during this process, as well as a small amount of CH_4 . Relative yields of CO and CH_4 as functions of time are plotted in Figure 2.18. The $[CO]/[H_2]$ ratio approaches an asymptotic value $\neq 0$, which is probably the result of a trace amount of H_2O formed in the copper turnings O_2 trap; this problem may be obviated by installing a molecular sieve trap (kept at $-196^\circ C$) after the O_2 trap.

Further work is being done in these areas, especially with regard to the role played by the iron impurity in the oxidation process.

2.4 Cesium Interaction With Materials (S. Nicolosi)

The investigation of an apparent reaction between CsI and air at elevated temperature is continuing. Since CsI from different sources yielded different results, the use of a purified reagent, with lot analysis data, was considered to be desirable.

Purified CsI (99.9%) has been obtained from Cerac/Pure, Inc. Primary impurities (lot number 6272-A-1) are Na, 200 ppm; Rb, 140 ppm; insoluble, 100 ppm; K, 50 ppm; and Li, 10 ppm. The sample was heated in dry flowing air (~ 1 SCFH) at approximately $630^\circ C$. A yellow-orange deposit was collected in a cold trap downstream of the sample. This material was collected after various runs by dissolution in methanol followed by vacuum distillation in a glass vial. In this manner, enough material has been obtained, so that a power X-ray diffraction analysis can be performed.

2.5 Helium Impurities Loop (L. Epel)

The Helium Impurities Loop (HIL) renovation was completed during this reporting period. A high-temperature valve was welded into the line between the graphite bed and the test retorts to isolate graphite from the loop if so desired. The old high-volume, low-temperature circulating pump was decommissioned and a metal bellows pump, capable of operating at $450^\circ F$, was received and installed. This eliminated the major source of leakage in the system as well as a major source of contamination with oil. Infra-red CO , CO_2 , and CH_4 meters were connected to the HIL as was an H_2O probe. The data acquisition and alarm system was incorporated into the loop and tested.

The entire system was leak tested with helium in a static condition. The

Figure 2.18. Relative yields of CO and CH₄ versus time during the hydrogenation of an oxidized PGX graphite specimen by 8.5% H₂ in He at 750°C.

only source of leakage is in the infrared meters which contain some plastic tubing. It is evident that smaller molecules such as H_2 and He can diffuse through this tubing and so this leakage can be eliminated only by replacing the tubing, which is internal to the instruments.

The loop was conditioned after leak testing to remove any oxygen from the metal walls. Hydrogen (in helium) was circulated at operating temperatures for several days and then the purification system was used to remove the accumulated water vapor and other impurities. This was repeated until the measured amount of water vapor found in the gas was nil.

After evacuating the loop and recharging with fresh helium another leak test was conducted. This time the loop was run at normal operating temperatures and normal flow rates, first with the infrared instruments valved out and then with them connected to the loop. Figure 2.19 shows the total pressure in the loop just downstream of the pump as a function of test time. From 0 hours to about 68 hours there is some scatter but no discernible trend in the pressure. The maximum deviation in observed pressures during this time period is equivalent to a $4^\circ F$ temperature variation in the helium. At 68 hours the infrared meters were valved into the loop. The 1.5% decrease in pressure is indicative of the volume of gas required to fill the connections to these instruments. For the next 100 hours there is a definite downward trend in pressure which is a manifestation of the leakage through the tubes in the meters. The leakage rate is estimated at 1% per day (actually 0.0394%/hour).

At the conclusion of this run (C-1) the IR meters were used to analyze the circulating gas for CO and CO_2 . Surprisingly, CO was present at a concentration of about 600 ppm and CO_2 at a concentration of 500 ppm. This experimental artifact is difficult to explain since the circulating helium was isolated from the purification system, the graphite bed, and any external source of CO and/or CO_2 . Experiments to investigate this phenomenon further will be conducted during the next quarter.

2.6 Safety Instrumentation and Control Systems (S. Nicolosi)

2.6.1 Impurity Monitoring

An EG&G optical dew point hygrometer (Model 440-S3) is operational and awaiting installation into the MTL. In the meantime, a few comparison measurements with Panametrics probes have been performed. Some typical results are:

<u>Panametrics Probe No.</u>	<u>Panametrics Probe (ppmv)</u>	<u>EG&G (ppmv)</u>	<u>$\Delta\%$</u>
11392-H	181.5	263	-31
11392-H	10,000	7800	-87
13559-H	789	578	+37

Figure 2.19. Leakage Characteristics of the Helium Impurities Loop.

The Beckman Model 865 infrared monitors are installed in the HIL and continuously monitor for CO, CO₂, and CH₄ levels. The calibration curves accompanying these instruments have been verified with Matheson standard gases. It has been observed that the infrared instruments are quite sensitive to pressure fluctuations necessitating careful pressure measurement and control of this manifold during loop operation. Another characteristic of these monitors is the permeability of helium through lengths of flexible teflon tubing internal to the instrument. This results in a small, continuous leakage of helium from the system which must be continuously replenished.

Interfacial equipment for our Varian gas chromatograph has still not arrived. This will cause further delays in the automatic sampling for hydrogen by gas chromatography.

The purchase of a modified Varian helium leak detector for the continuous measurement of hydrogen is being requested for HIL loop kinetics experiments.

2.6.2 Monitoring for Impurities in the Primary Coolant at Fort St. Vrain

The mass spectrometer to be used for this task is still under construction by the Nuclide Corporation in State College, Pennsylvania.

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3. Structural Evaluation

3.1 Core Seismic (P. Bezler, M. Reich, H. Goradia)

Efforts in this report period have been directed towards the corroboration of the multi-column vertical array code. For this purpose a number of tests were conducted with the vertical array shake test rig and duplicated analytically. All shake test runs were recorded on high speed motion pictures while the analytical results were recorded using the computer graphics option.

These tests were limited to horizontal sinusoidal excitation of a single column of ten elements at constant forcing frequencies of 6, 8, and 10 cycles/sec. The forcing amplitude was held constant at 0.5 inches peak to peak while the total horizontal clearance was 0.9 inches. For the code runs the input spring-damper parameters were set to match those of the physical rig with, however, some uncertainty in the damping characteristics. Computer runs were limited to modeling two seconds of real time.

A comparison of the two sets of results indicated that the computer code correctly predicted the characteristics of the response motion. The duration of the transient was short with near steady state motion evident after only two seconds. The predicted and observed steady state mode shapes were near identical. The break up of the stack at the highest frequency, the upper two blocks disengage from the lower blocks and execute free motion, was correctly predicted. In short good overall corroboration was obtained.

With the test rig, in addition, exploratory tests were carried out with the single column over a wide frequency range. Both sweeping sinusoidal and fixed frequency sinusoidal excitation was used. The occurrence of the jump phenomena was easily observed. Both high speed and regular speed motion pictures were taken. The start and stop characteristics in the region of multiple roots, for this nonlinear system, were easily developed.

As another extension of the shake test program, the effects of wholly vertical excitation on the single stack array was investigated. To do this the vertical array test rig was suspended and supported from the ceiling with spring hangers to remove the weight of the system from the shaker. The shaker was mounted in the vertical mode, centered directly under the stack and rigidly clamped to the test rig base plate.

The stack model was found void of any significant response characteristics attributable to vertical excitation except for the tendency of the column to disengage. Although a whole range of sinusoidal inputs were investigated, the observed response was the same for all tests. The response was negligible until the peak acceleration level of the input approached 1 g. At this level, complete stack break up occurred and the test was terminated. An extension of these tests to investigate the effects of simultaneous horizontal and vertical excitation is under way.

In the basic studies, the effect of damping on the response of the cubic system excited by multiple sine waves was further studied. As described in previous reports, the critical ratio of the coefficients of the forcing function was examined for different degrees on nonlinearity. In this report period the effect of the damping ratio on the occurrence of the critical discontinuity was studied.

The results are shown in Figure 3.1. The general characteristic shape of these curves is similar to those reported in the last quarterly for changes in the level of the F_2 coefficient. Three sets of damping curves are shown for three different values of the nonlinear term β . For each value of the nonlinear coefficient β , four different damping levels are shown. In all cases for the curves in Figure 3.1, the value of F_2 is 200.

Figure 3.2 shows the area of discontinuous response in relation to the area of stable response which occurs for large ratios of F_1/F_2 . These curves are for a sweep up in frequency. As the ratio of F_1 to F_2 increases, the effect of the primary term becomes more and more significant. For small values of F_1 , however, the effect of the subharmonic is dominant. For example, for $F_2 = 100$, the effect of the subharmonic response is greater than the primary term up to a ratio of F_1/F_2 of .05 ($F_1 = 5$).

3.2 Steam Generator Tube Weld Evaluation (M. Reich, M. Subudi, T. Tanaka, P. Bezler)

An investigation of the structural integrity of the HTGR steam generator tubes has been initiated. The principal function of the steam generator tubes is to provide a surface through which thermal energy may transfer from the primary side to the secondary side coolant under both normal and abnormal operating conditions. A failure in these tubes will result in nuclear contamination of the secondary fluid. Previous studies have indicated that cracks may initiate in the tube weldments when they are subjected to the normal design pressure and temperature loadings. A critical study of these tube welds has been undertaken.

As presently envisioned, the study is divided into three phases. In the first phase only elastic analysis methods will be employed. These will be used to qualitatively identify the influence of material combinations on the magnitudes and distribution of stress in the joints, permit an assessment of their service integrity in accordance with the ASME Code criteria and, by the introduction of cracks in the high stress regions, study crack effects using Linear Elastic Fracture Mechanics (LEFM) methods. In the second phase, inelastic time-independent analyses of the weldments will be performed. This will include welding operation induced residual stress evaluations, thermal ratchetting studies and severe transient evaluations. In these analyses it can be assumed that gross plastic yielding will occur and that the LEFM techniques will not apply. Other methods to investigate the failure behavior will be employed. In the last phase, an attempt will be made to study high temperature, time-dependent inelastic behavior (creep, creep fatigue, etc.) of the weldments. Reasonable assumptions will be made to characterize the results in accordance with the present techniques for time dependent life predictions.

The phase I study has been initiated during this report period. Literature surveys of the relevant material have been undertaken. The necessary analytical methods; an elastic, finite element structures code, a transient finite element heat transfer code, a linear elastic fracture mechanics code and a fatigue life evaluations code are being tested and brought to operational status.

Some of the causes of joint failures Carpenter (1950) and Blaser (1950) are thought to be the difference in the thermal expansion between different materials, intergranular stress oxidation which leads to the formation of oxide filled notches in the high stressed regions and weakening of the material adjacent to the fusion zone due to carbon migration. Welding operation induced residual stresses are also felt to be of paramount importance Friedman (1975) and Dalcher (1976).

EFFECT OF $\frac{c}{c_c}$ AND β ON MAXIMUM RESPONSE

FIGURE 3.1

EFFECT OF MAGNITUDE OF F_1 AND F_2 ON MAXIMUM RESPONSE

FIGURE 3.2

Of the codes being readied, the structures code is a conventional finite element code fully operational on the BNL system. The heat transfer code is newly acquired and undergoing debugging runs. Based on a finite element formulation, it will be particularly useful when thermal transient effects are studied, permitting a direct match up to the structures code. The linear elastic fracture mechanics code, also a finite element formulation, is designed to calculate both the mode I and mode II stress intensity factors (K_I , K_{II}) for cracks in plane or axisymmetric structures. It is also in the debugging and prooftesting phase, some options functioning correctly, others malfunctioning. The fatigue life evaluations code, essentially a crack growth prediction code, has been implemented on the BNL system but must still be put to rigorous prooftesting.

Although only proof-test runs have been made with the LEFM code, two general conclusions can be drawn from the results: the predicted stress intensity factors are extremely sensitive to crack location and prevailing boundary conditions, and the computed factors are all of very small magnitude. Regarding the first, we intend, in the future studies, to investigate numerous crack locations while matching exactly the displacement boundary conditions predicted by the elastic analyses. Regarding the second, it may be inferred that only ductile, fully plastic, limit load type failure modes might be expected in the structures. This conclusion, however, is directly dependent on the magnitude of the critical stress intensity factors for the materials under consideration. These quantities have yet to be determined and, particularly for environment affected weld filler metal, may be very low.

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4. Analytical

4.1 Thermal-Hydraulics During Natural Convection (L. G. Epel)

The computer program that solves transient thermal-hydraulic problems in circulating gas systems has been completed. The code SODEMME (Solution of One-Dimensional Energy, Mass, and Momentum Equations) is written in modular form, with all subroutines except the one that is concerned with heat transfer in various components of the circuit, applicable to generalized geometrics. The latter subroutine was written during this reporting period specifically to compute steady state and transient heat transfer in components of a 3000 MWth HTGR. This first version of SODEMME is thus partly "hard-wired" to the HTGR design; however, other systems can be simulated in the future by altering only the heat transfer subroutine and leaving the remainder of the coding intact.

Since the work during this quarter concentrated on the heat transfer subroutine HTRNSFR, the models incorporated into that part of the code will be presented here. For each of the four major components modeled, three conflicting considerations had to be reconciled: accuracy of the model, simplicity of the formulation and execution of the algorithms, and freedom from the burden of input preparation for each run of the program. It goes without saying that many compromises had to be struck between these three criteria.

Two important parameters in the program are the heat transfer surface temperatures and the rate of heat flow to the circulating helium in each of the segments that comprise the system. HTRNSFR supplies these quantities for each segment by providing four options during computation of the steady state and five options during computation of transients. Control is then returned to the calling subroutine. It should be recalled that besides these options, the user may specify the rate of heat transfer to helium in any segment explicitly as a function of time. The purpose of HTRNSFR is to offer additional methods of specifying boundary conditions. An attempt has been made in writing the subroutine to anticipate probable user needs when he defines a given problem.

4.1.1 Steady State Options

Option 1 - The first option available in HTRNSFR is the only one not specifically tailored to any particular kind of HTGR component. It calculates heat transfer in a constant flow area channel having a constant surface temperature. If it is assumed that the heat transfer coefficient and gas properties are independent of position along such a channel, the outlet gas temperature can be found in terms of the other parameters of the flow system:

$$T_o = T_s + (T_i - T_s) \exp - (hpl/w c_p) \quad (4.1)$$

where

T_o = channel outlet helium temperature
 T_s = channel surface temperature

T_i = channel inlet helium temperature
 h = heat transfer coefficient
 p = wetted perimeter of channel
 l = length of channel
 w = rate of helium flow
 c_p = specific heat of helium.

For this option the channel surface temperature must be specified during input. The heat transferred into the gas is simply calculated from

$$q = w c_p (T_o - T_i). \quad (4.2)$$

Option 2 - This option is used to calculate heat transfer and surface temperatures in ducts lined with the thermal barrier used in the HTGR. Thus, it is appropriate to use this option when it is desired to simulate the cross ducts that connect the plena to the steam generators or to the CACS.

It is assumed that the thermal barrier consists of a 1/4-inch-thick Hastelloy cover plate over a 2 3/4-inch-thick Kaowool blanket. The side of the Kaowool blanket away from the cover plate is cooled by water circulated through the liner cooling system. It is assumed that this side is always maintained at 100°F. The thermal conductivity of the Kaowool is taken to be linear with temperature over the range likely to be of interest in practical applications:

$$k = a + b T. \quad (4.3)$$

By matching the heat flux across the interface between the helium and the cover plate to the heat flux through the insulation, the cover plate temperature (i.e. the surface temperature) can be determined. Continuity of heat flux results in the quadratic:

$$\frac{b}{2\Delta x} T_s^2 + \left(\frac{a}{\Delta x} + h\right) T_s - \frac{a}{\Delta x} T_c - \frac{b}{2\Delta x} T_c^2 - h T_i = 0 \quad (4.4)$$

where

T_c = coolant temperature = 100°F
 Δx = thickness of Kaowool.

Once the surface temperature is obtained the heat transferred to the helium is found from the convection equation:

$$q = hpl (T_s - T_i). \quad (4.5)$$

Option 3 - This option considers heat transfer in the plena of the HTGR. The plenum is modeled as two circular surfaces, one a graphite surface (the reflector), and the other a thermal barrier having exactly the same characteristics as the thermal barrier lining the ducts in Option 2. The heat transfer and surface temperature calculation for the latter is thus, in principal, the same as in Option 2.

In this option, however, the amount of heat transferred from the reflector to the helium must also be specified during input preparation. This amount of heat is arbitrary and assumed independent of the heat transfer situation prevailing in the thermal barrier. The surface temperature of the reflector is then simply obtained from the convection equation. Finally, the surface temperature of the graphite is compared to the surface temperature of the cover plate on the thermal barrier. The lesser of these two temperatures is printed in the output as the relevant surface temperature.*

Option 4 - This option is very similar to Option 3 in that heat transfer in the plena is considered once more. The only difference between this model and the one in Option 3 is that in this model the surface temperature of the reflector, instead of the amount of heat transferred from the reflector, must be specified during input preparation. The heat transferred from the thermal barrier is again computed as in Options 2 and 3 and the heat transferred from the reflector is computed from the convection equation. Here again, the reported surface temperature is the smaller of the two surface temperatures (i.e. the thermal barrier cover plate or the graphite reflector).

4.1.2 Transient Options

These options compute transient heat transfer in various components of an HTGR. The first four options are the transient analogs of the four options outlined above in the section on steady state models. The fifth one is specific to the steam generators in the HTGR. The first step is to determine the steady state solutions consistent with the steady state boundary conditions and then use these solutions as the initial conditions for the transient portions. Since an explicit method of solution is employed some attention must be paid to the time step used so as not to violate stability criteria. The equations used in these options will be outlined in some detail so that the limitations on the allowable time step will be apparent. It will be seen that the upper limit on the time step for transient heat transfer calculations is many times larger than the time step that would normally be used for the other parts of the SODEMME program and so this consideration does not impose an undue restriction on the user.

Option 1 - Figure 4.1 shows a representative unit cell from the core of an HTGR. Although the thermal problem is two-dimensional in principle, studies have shown that in practice it is basically one-dimensional. It happens that the graphite in the right-angle region of the unit cell is not influential in conducting heat from the fuel to the coolant and can be almost completely neglected. This option, therefore, utilizes the pseudo-one-dimensional equivalent unit cell shown in Figure 4.2. In this equivalent cell the fuel rod diameter has been kept intact, the lengths of the fuel-graphite interface and the coolant-graphite interface are preserved, and the thickness of the graphite web is left unchanged. The only important parameter that is altered is the volume of graphite. Since this is important only as a thermal inertia term during transients, the thermal diffusivity

*The lower surface temperature is relevant in this application because interest is focused on the possibility of fission product condensation on PCRV internals.

Figure 4.1. HTGR unit cell.

Figure 4.2. Equivalent one-dimensional unit cell.

of the graphite was modified to compensate for the volume change resulting from the mapping into an equivalent cell.

In this conduction model the fuel is divided into five concentric cylindrical regions of equal width and the graphite is divided into four equal width slabs. The steady state, finite difference solution for each of the resulting ten nodal points is easily obtained by application of the Poisson Equation. The transient heat conduction equation for an internal point, using forward differences, is in general:

$$k_l A_l \frac{T_l - T}{S_l} + k_r A_r \frac{T_r - T}{S_r} + gV = \rho C_s V \frac{T' - T}{\Delta t} \quad (4.6)$$

where

k = thermal conductivity
 A = area for heat transfer
 S = distance between nodes
 g = heat generation rate
 V = volume of solid
 ρ = density of solid
 C_s = specific heat of solid
 Δt = time increment

and the prime notates the new condition after the time step has taken place. Solving this equation for T' results in a linear equation in T_l , T_r and T in which the coefficient of T is:

$$1 - \frac{k_l A_l}{\rho C_s S_l} \Delta t - \frac{k_r A_r}{\rho C_s S_r} \Delta t. \quad (4.7)$$

For reasons of numerical stability this coefficient must always be positive and that consideration yields the limitation on the size of the time step:

$$\Delta t \leq \frac{\rho C_s V}{\frac{k_l A_l}{S_l} + \frac{k_r A_r}{S_r}}. \quad (4.8)$$

Similarly, for a point on the graphite-coolant interface, a similar heat balance results in another stability criterion:

$$\Delta t \leq \frac{\rho C_s V}{\frac{k_l A_l}{S_l} + h A_r}. \quad (4.9)$$

For the numerical values appropriate to the HTGR core materials and geometry the most restrictive of these conditions occurs at the graphite-coolant interface (Eq. 4.9) but even there the critical time step is about 175 milliseconds. This is two orders of magnitude higher than the typical time step used in the other sub-routines in SODEMME and so is not in any sense debilitating in terms of executing the code.

Option 2 - This option computes the transient behavior of the thermal barrier lining the duct walls in an HTGR. The same assumptions regarding size and thermal properties of the barrier that are used in Option 2 during steady state are used again here.

First the steady state temperature is determined in order to establish the initial conditions. Then the transient heat transfer problem is solved using the same finite difference technique as before. The heat conduction situation in the thermal barrier is described, in principle, by Eq. (4.6) but it is noted that the heat generation term vanishes and the thermal conductivity becomes temperature-dependent. The Kaowool is divided into nine equal intervals and the cover plate is assumed to be isothermal. The stability criterion for interior points is similar to Eq. (4.8), but because the thermal conductivity is a function of temperature this criterion is somewhat more complicated. For practical purposes, however, Eq. (4.8) gives a sufficiently accurate upper limit to the time step. Similarly, for the cover plate temperature calculation the stability criterion is given with sufficient accuracy by Eq. (4.9). Both of these expressions involve the thermal conductivity of the Kaowool, which is dependent on temperature. Hence, the criteria are temperature-sensitive and an estimate of prevailing temperatures is necessary to evaluate the maximum size time step that is allowable. As before, for any practical set of numerical values relevant to an HTGR, the time step is generally on the order of 100 times the time step commonly used in the rest of the program.

Option 3 - This option is the time-dependent analog of Option 3 among the steady state algorithms. The thermal insulation temperature is found as a function of time exactly as in Option 2 above; i.e. by first obtaining the steady state solution and then stepping along in time using a finite difference technique. The contribution of the reflector to the heat input is specified as a function of time explicitly and so HTRNSFR simply adds this component to the heat transferred from (or to) the thermal barrier.

The surface temperature of the thermal barrier is determined as part of the transient solution. The surface temperature of the reflector is obtained from the convection equation. As in the steady state situation, the lesser of these two surface temperatures is printed in the output under that designation.

Option 4 - This option is the time-dependent analog of Option 4 among the steady state algorithms. The treatment of the thermal insulation is once again as in Option 2 above but it is the surface temperature of the reflector, rather than its heat flow, that is specified in the input as a function of time; the convection equation supplies the latter quantity. The surface temperature that is printed is the lesser of the two surface temperatures (i.e. graphite surface or thermal barrier surface), as before.

Option 5 - This option provides a simple model for the main steam generators during transients. There is no steady state analog; the heat transferred during the steady state portion of the computation must be a constant specified as input. If the heat transfer during the transient portion is not specified as some constant or as an explicit function of time, this option will compute heat transfer in the

steam generators based on the following premises:

- . The steam generators and reheaters are homogenized into 6 parallel and uniform 72-foot-long assemblies containing a total of 3.15×10^6 pounds of steel and having a total of 201,600 square feet of heat transfer surface.
- . The steam generators and reheaters have no water or steam in the modules. Any heat transfer that takes place is between steel and helium.
- . There is no temperature variation radially through the tube and/or shell walls. All resistance to heat flow takes place in the helium boundary layer.

The programming is such that any initial steel temperature may be specified during input to serve as the initial condition for each segment using this option. If an initial temperature is not specified, the code proceeds by assigning the helium gas temperature to the steel. The transient portion is a straight-forward finite difference approach without the complication of requiring temperature profiles to be calculated through the metal walls. Specifically, the heat balance for any segment is:

$$h A (T_f - T_s) = M C_s \frac{T'_s - T_s}{\Delta t} \quad (4.10)$$

where

M = mass of steel in a segment.

This can be rewritten to give T'_s explicitly in terms of the other variables. When this is done the stability criterion for this option becomes evident:

$$\Delta t \leq \frac{C_s M}{h A} \quad (4.11)$$

As in all of the previous stability criteria, when realistic numerical values are substituted into Eq. (4.11), the resulting time step is on the order of 100 times the time step normally used in the other subroutines of SODEMME.

4.2 HTGR Safety Code Library (J. Beerman, J. Colman)

The computer codes CHAP (Composite HTGR Analysis Program) (Gilbert 1976), ISOBAR and DFF-MH69 have been received from Los Alamos Scientific Laboratory. CHAP is currently undergoing conversion to the BNL CDC 7600 computer.

The code SWIRL (Gosman 1969) is being adapted for use on the BNL CDC 7600 computer. The steady state version, SWIRL-S is operational; a transient version, SWIRL-T is being developed. The code is described more fully elsewhere in this report.

The code EVAP is being developed at BNL to calculate evaporation and re-distribution of fission products from the core during a temperature transient.

The codes SODENME and CIRC, reported in the previous quarterly (Schweitzer 1977) is now operational. NOTE: The code CIRC was called JETS in the previous HTGR Code Library Listing (Schweitzer 1977), though it was correctly referred to as CIRC elsewhere in that report.

Plotting capability has been added to the KACHINA code. Previously, the SC4020/FR-80 plotting contained in the original LASL-developed code was not operational at BNL. In addition, velocity-vector plots have been added to the KACHINA code.

Table 4.1 summarizes the status of the 56 codes now being developed or maintained in the HTGR Safety Code Library.

4.3 Diffusion of Fission Products Through the Graphite After a Loss of Coolant Accident (J. M. Dickey)

The SORS (Schwartz 1974) code calculates the rate of release of the fission products from the core following a transient temperature excursion. As the core temperature rises the fuel particles eventually fail. The volatile fission products are released directly to the primary coolant but the non-volatile fission products must diffuse through the graphite moderator and evaporate at the coolant channel surface. The version SORSG calculates the release rate for the non-volatile fission products. The geometry of the core is very complex so to make the problem tractable, a simple cylindrical geometry is used. The evaporation at the surface of a coolant channel is a boundary condition for the diffusion equation but to simplify the problem further the diffusion and evaporation processes are treated independently in SORSG. The approximations used in the treatment of diffusion have been discussed in the previous progress report. Here, we wish to discuss the calculation of the rate of evaporation. It is assumed in calculating the rate of evaporation that diffusion is sufficiently rapid that the concentration at the surface is the same as throughout the surrounding graphite.

After a loss of coolant accident the helium flows slowly through the core because of natural convection. A necessary input parameter for SORSG is the coolant flow rate. This has been estimated by General Atomic Company in GA-LTR-10 using the code RECA (Chi 1970) to be about 0.034 lb/h at 0.1 hours and about 0.000053 lb/h at 50 hours. As the coolant flows up the coolant channels the impurities will evaporate from or be deposited back on the graphite wall depending on whether the partial pressure in the coolant is greater than, or less than, the partial pressure at the surface of the channel. As the core heats up, material will initially evaporate from the hotter central regions and be redeposited at the cooler regions at the top and the reflector. Eventually these regions heat up and the material re-evaporates. In SORSG each coolant channel is divided into a number of axial segments in each of which the temperature is taken to be uniform and also the core is divided into several radial regions so that several different coolant channels are modeled. The temperature history of each section of the core, following the accident, must be supplied as input to SORSG. For a loss of coolant accident the code CORCON is used to calculate the temperature versus time.

TABLE 4.1

HTGR Safety Code Library

<u>Program</u>	<u>Status</u>	<u>Proprietary</u>	<u>Function</u>
CRECT	OP	NP	Corrects data on ENDF/B tape.
LISTFC	OP	NP	Lists data from ENDF/B tape.
RIGEL	OP	NP	Manipulates data on ENDF/B tape.
ENDFB2	OP	NP	Converts data on ENDF/B tape to binary.
FLANGE	OP	NP	Prepares thermal cross section transfer arrays.
GAND2	OP	NP	Prepares fine group fast, resonance, and thermal cross sections from ENDFB2 binary tapes.
GFE2	OP	NP	Prepares fine group fast cross sections from ENDFB2 binary tapes.
MAKE	OP	NP	Prepares fine group fast cross section tape from GFE2 for spectrum codes.
WTFG	OP	NP	Prepares fine group thermal cross section tape from GAND2 or FLANGE for spectrum codes.
PRINT	OP	NP	Reads the fast cross section tape produced by MAKE.
SPRINT	OP	NP	Reads the thermal cross section tape produced by WTFG.
GGC4	OP	NP	Prepares broad group cross sections from MAKE and WTFG tapes.
INTERP	OP	NP	Prepares broad group cross sections from MICROX output data tapes.
1-DX	OP	NP	Performs one-dimensional, diffusion theory, steady state calculations.

C = In the process of being converted to the CDC 7600.

OP = Operational on the BNL CDC 7600 system under Scope 2.

R = Recently received from General Atomic Co. or Argonne Code Center.

P = General Atomic Co. proprietary code.

NP = Not considered to be a proprietary code.

B = In the process of development at Brookhaven National Laboratory.

TABLE 4.1 Cont'd.

<u>Program</u>	<u>Status</u>	<u>Proprietary</u>	<u>Function</u>
FEVER-7	OP	NP	Performs one-dimensional, diffusion theory, burnup and reload calculations.
TEMCO-7	OP	NP	Computes reactor temperature coefficients from input cross section data.
BLOOST-7	OP	NP	Performs zero-dimensional reactor kinetics calculations.
GAKIT	OP	NP	Performs one-dimensional reactor kinetics calculations.
TWIGL	OP	NP	Performs two-dimensional light water reactor kinetics calculations.
TAC-2D	OP	NP	Performs two-dimensional, transient conduction analyses.
FLAC	OP	NP	Calculates steady state flow distributions in arbitrary networks with heat addition.
POKE	OP	P	Calculates steady state flow distributions and fuel and coolant temperatures in a gas cooled reactor.
PREPRO	OP	P	Prepares input data and source code revisions for RECA code.
RECA	C	P	Calculates time dependent flow distributions and fuel and coolant temperatures in the primary system.
CORCON	OP	P	Computes the temperature history and fission product redistribution following a loss of all convective cooling of the core.
SORSO	OP	P	Computes the release of volatile fission products from an HTGR core during thermal transients.
GOPTWO	OP	NP	Analyzes the steady state graphite burnoff and the primary circuit levels of impurities.
OXIDE-3	OP	P	Analyzes the transient response of the HTGR fuel and moderator to an oxidizing environment.

TABLE 4.1 Cont'd.

<u>Program</u>	<u>Status</u>	<u>Proprietary</u>	<u>Function</u>
SAMPLE	OP	NP	Propagates uncertainties in probability distributions by Monte Carlo technique.
TAP	OP	P	Calculates the transient behavior of the integrated HTGR power plant.
SORSQ	OP	P	Computes the release of non-volatile gaseous fission products from an HTGR core during thermal transients.
DIFFTA	OP	NP	Solves the steady state, two-dimensional diffusion equation by finite element method.
DIFFTL	OP	NP	Solves the time dependent, 2-D diffusion equation by the finite element method.
PDQ-7	OP	NP	Analyzes reactors in up to three space dimensions via diffusion theory.
SOSNCC	OP	NP	Calculates steady state natural convection flow rates during long term loss of power.
HILDA	OP	NP	Analyzes data acquired from the Helium Impurities Loop.
OSCIL	OP	NP	Simulates one-dimensional spring mass system for seismic analysis of HTGR core.
H-CON1	OP	NP	Calculates one-dimensional heat conduction for an HTGR fuel pin by finite difference method.
HYDRA-1	OP	NP	Computes thermal/hydraulic parameters for single phase liquid due to external heat source.
CONTEMPT-G	OP	P	Simulates temperature-pressure response of an HTGR containment atmosphere to postulated coolant circuit depressurization.
SOSSME	OP	NP	Solves the steady state one-dimensional momentum equation.
HAZARD	OP	NP	Analyzes gas layering and flammability in an HTGR containment vessel following a depressurization accident.

TABLE 4.1 Cont'd.

<u>Program</u>	<u>Status</u>	<u>Proprietary</u>	<u>Function</u>
CIRC (formerly JETS)	OP	NP	Calculates fluid dynamics in an HTGR containment vessel following a depressurization accident.
ORIGEN	OP	NP	Solves the equation of radioactive growth and decay for large numbers of isotopes with arbitrary coupling.
MOSBDA	OP	NP	Analyzes experimental data from Mössbauer spectrometer.
INREM	OP	NP	Calculates internal radiation doses.
EXREM	OP	NP	Calculates external radiation doses.
SODENME	OP	NP	Calculates transient thermal hydraulic aspects of circulating gas systems.
KACHINA	OP	NP	Calculates fully interpenetrating fluid flows using Implicit Multifield (IMF) method, for handling different material fields.
SWIRL-S	OP	NP	Calculates fluid dynamics for two-dimensional steady state recirculating flows.
SWIRL-T	B	NP	Calculates fluid dynamics for two-dimensional time-dependent recirculating flows.
CHAP	C	NP	Simulates the overall HTGR plant with both steady state and transient solution capabilities.
EVAP	B	NP	Calculates evaporation and redistribution of fission products during a temperature transient.

After the fuel particles fail, the fission products are released to the graphite binder and then must diffuse across the graphite matrix to the coolant channel. In order to calculate the evaporation rate, the assumption is made in SORSG that the distribution of the impurity is uniform throughout each section of the core. If all the impurity present in a given section has originated within the fuel particles contained in that section, this is a conservative assumption, since the concentration at the coolant surface must be less than in the interior; however, this assumption may not be realistic at a temperature at which the diffusion rate is low. If we consider a portion of the top reflector, initially there are essentially no fission products present. However, as the core heats up fission products will evaporate from the hot central portions of the core, will be carried up the coolant channel and will redeposit back on the top reflector. To calculate the rate of deposition onto the top reflector the same assumption concerning diffusion is made; namely, that the concentration becomes uniform instantaneously. However, in this instance the direction of diffusion is in the opposite direction, from the coolant channel surface toward the interior of the matrix graphite. Moreover, the reflector is at a lower temperature than the rest of the core so diffusion rates are slower. The concentration at the surface will actually be higher than that calculated using a uniform concentration and there may be a buildup of impurity at the surface especially for elements and temperatures for which the diffusion rate is low. Thus, the effect of the assumption of infinitely fast diffusion is, in this case, to overestimate the rate of deposition, i.e. to underestimate the probability of an impurity atom being carried out of the coolant channel, and so this assumption is not conservative in this instance.

It is usually assumed that the impurity atoms in the immediate vicinity of a surface are in equilibrium with the atoms on the surface so the driving force for mass transfer between the surface and the coolant is the difference between the vapor pressure of the impurity at the surface and the partial pressure in the coolant stream. In general, one writes (Bird 1960):

$$R(t) = k A \Delta P \quad (4.12)$$

where

$R(t)$ = rate of evaporation or deposition
 k = mass transfer coefficient
 ΔP = driving force
 A = surface area.

The mass transfer coefficient depends on the geometry of the channel and the flow characteristics and is usually adapted from results for heat transfer by making appropriate substitutions (Bird 1960). After the loss of coolant accident very low flow rates characteristic of natural convection would be expected and the range of Reynolds numbers was estimated by the General Atomic Company in GA-LTR-10 to be from 6.9 to 0.0041. The other relevant parameter determining which expression should be used in the mass transfer coefficient is the dimensionless quantity $N = L/(d Re Sc)$ where L is the length of a segment, d its diameter and Re and Sc the Reynolds and Schmitt numbers. This quantity is a measure of the stagnation of the flow and can be rewritten as a ratio of the time taken for the coolant to

traverse the channel to the time taken for an impurity atom to diffuse from the wall to the center of the channel. Using the parameters involved in SORSG a typical value of N is about 1000. This corresponds to almost stagnant flow conditions and in this limiting case the partial pressure at the end of a segment is approximately equal to the vapor pressure, and the logarithmic Nusselt number has the limiting value of 3.66 (Bird 1960; Sherwood 1953).

In SORSG the rate of mass transfer is written in the form (5.9) (Schwartz 1974):

$$R(t) = H W (P_b - P_c) / P_s \quad (4.13)$$

where

- $R(t)$ = evaporation or condensation rate (g/sec)
- H = local mass transfer coefficient
- W = carrier gas flow rate (g/sec)
- P_s = total system pressure (atm)
- P_b = vapor pressure at graphite surface (atm)
- P_c = element partial pressure in the coolant stream at the entrance to the core segment (atm).

In Section 5.3.3 of LTR-10, for three different flow regimes, expressions for the Stanton number are given, and H in Eq. (4.13) is equated to the Stanton number. Only the third of these expressions is applicable to the conditions of a loss of coolant accident so:

$$H = 3.66 / (\text{Re } \text{Sc}). \quad (4.14)$$

A correction factor, Eq. 5.20 of LTR-10, is applied to account for the diffusion of the impurity through the boundary layer, since this is approximately 1 for low impurity concentrations we will omit it here. Combining these equations, the expression used in SORSG for the mass transfer rate is:

$$R(t) = \text{St } W (P_b - P_c) / P_s \quad (4.15)$$

which reduces to:

$$R(t) = (3.66 / \text{Re } \text{Sc}) W (P_b - P_c) / P_s \quad (4.16)$$

for the flow conditions expected. These equations are in error. The correct expressions for the mass transfer rate in terms of the Stanton number is (Bird 1960):

$$R(t) = \text{St } W (4L/d) (P_b - P_c) / P_s \quad (4.17)$$

and under the limiting conditions of very slow flow:

$$R(t) = W (P_b - P_c) / P_s. \quad (4.18)$$

In Eq. (4.15) a geometrical factor has been omitted and in Eq. (4.16) a limiting value for the logarithmic Nusselt number has been combined with a pressure difference involving the inlet partial pressure. The net result is that the expression used for $R(t)$ in SORSG contains an erroneous factor equal to the Stanton number. The implications of this on the further calculations cannot be predicted. For example, with the trial data supplied with the code SORSG a typical value of St is about 10. Thus, in one portion of the core the code will overestimate the rate of evaporation by a factor of 10, yet in another portion of the core where mass transfer is in the opposite direction, the code will overestimate the amount deposited back on the surface by a factor of 10. So the net effect of the error on the release rate of a fission product from the core is unpredictable. To see what the effect may be the instructions in the code SORSG in which the evaporation rate was calculated, were changed from Eq. (4.16) to Eq. (4.18). The rest of the structure of the code was unchanged. Figure 4.3 shows the output using the trial data supplied by General Atomic for the two calculations. The release curve is shifted by about 10 hours for ^{90}Sr .

4.4 Gas Dynamics in the Containment Building (I. Omata, H. Takahashi)

The CIRC code was developed for the analysis of the gas dynamics of HTGR containment atmospheres during hypothetical accidents involving depressurization of the primary system. CIRC offers advantages over other codes for this problem. SOLA (Hirt 1975) solves incompressible fluid dynamics with no energy change by an explicit method, and KACHINA (Amsden 1974) solves dynamics of the two-phase fluid by an implicit method for the mass balance and momentum equations. CIRC was introduced, and temporarily named JETS, in the previous HTGR Safety Evaluation Division quarterly report.

Although CIRC is not yet completed, it was applied to HTGR containments. Since the code is unable to simulate the spherical dome nor three dimensional geometries, several cases were required to simulate air ingress into the jet (Case 2B), displacement of rupture location (Case 3B), and cylindrical geometry (Case 4B). Figure 4.4 shows schematic models of these three cases.

Case 2B was calculated in order to simulate air ingress into the jet through the jet boundary. The geometry used in the calculation is shown in Figure 4.6 of the previous quarterly progress report. The jet boundary was treated as an outflow boundary in the calculation. A grid of 4 x 8 cells in the x-y plane was applied and the jet was put in from the topmost left boundary cell. On the other hand, air was considered to flow out through the left boundary to the jet except at the topmost cell. The input velocity of the jet and the outflow velocity of air were obtained from the results of the HAZARD code (Omata 1977a). Figure 4.5 shows the circulation in Case 2B. It is interesting that the flow is about 6 m/sec even at 50 seconds and that closed circulation was only developed in the upper region comprising about two thirds of the containment. While Figure 4.5 shows a complicated temperature distribution, the pressure distribution remains almost stable. This may be because of the strong effect of gravitational forces.

Case 3B was calculated in order to see the effect of displacement of the rupture point. The actual location of the PCRV penetration closure would not be

Fig. 4.3 Release of Sr90 from the core calculated by the code SORSG using trial data.
(1) unmodified code, (2) equation for rate of evaporation changed.

CASE 2B

(EFFECT OF AIR INGRESS
INTO JET)

CASE 3B

(EFFECT OF THE
RUPTURE LOCATION)

CASE 4B

(EFFECT OF CYLINDRICAL
SHAPE)

Figure 4.4. Schematic models used in three cases.

Figure 4.5. Velocity vector field and contour lines of temperature and pressure in Case 2B.

the center of the PCRVR. Therefore, the circulation may be non-symmetrical. A grid of 8 x 8 cells in the x-y plane was used and the vertical velocity was put in from one of the bottom cells. Although the actual location of the shock disc shifts as time proceeds (Omata 1977a), its location was fixed at the bottom cell, as a first simple approximation. Figure 4.6 shows non-symmetrical flow pattern variation. At 10 seconds, all of the gas in the containment circulates as one stream, but after 20 seconds, most of the gas in the lower half has very little circulation. Temperature contour lines show non-symmetrical distribution but this distribution is not as complicated as in Case 2B. Pressure distribution, except during initial 10 seconds, remains almost stable in the same way as in Case 2B.

Case 4B was calculated in order to simulate flow in actual cylindrical geometry. A grid of 4 x 8 cells with symmetrical geometry was used and vertical velocity was put in from one of the bottom cells. In this case, the location of the shock disc was also fixed at the bottom cell as in Case 3B. Figure 4.7 shows the circulation in this case. During the initial 10 seconds, there is a flow along the wall but after that time there is no strong circulation. It may be reasonable to say that cylindrical geometry has a strong effect on circulation. We can also see the small closed circulation which continues almost all of the time. This continuous flow is due to neglecting the viscosity in the calculation. The temperature distribution is not as complicated as in Case 2B. The pressure distribution during the initial 10 seconds shows a sort of pressure impulse due to strong input flow, but afterwards its distribution becomes almost stable in the same way as in Cases 2B and 3B.

In summary, these simulations showed: complicated temperature distribution, very little circulation in the lower containment especially after the initial 10 seconds, and strong effects of gravitational forces and cylindrical geometry on circulation. Details of this application are included in a memorandum to the file (Omata 1977b).

Modification of the code for inclusion of heat transfer through the containment wall has started at the end of this quarter. One component gas dynamics is treated in the CIRC code. In order to analyze multicomponent gas dynamics with heat and mass transfer, the code GENMIX-4 developed by Spalding (1970) was adapted to the BNL CDC 7600 computer, and the steady state gas jet with chemical reaction was studied. Instead of the formalism used for the velocity in the CIRC code, the vortex formalism is used in GENMIX-4 and the pressure is eliminated from the formalism.

4.5 Thermal Liner of PCRVR: Flow Noise Excitation and Fatigue Failure Evaluation (H. Fenech, A. Rao, I. Ganz - University of California at Santa Barbara)

The thermal liner on the top face of the core upper plenum is under study with particular attention to the flow noise interaction with the cover plate.

Some preliminary estimations of the fatigue lifetime of the cover plate have been made using the program STREC-3 and the extreme case of a square simply supported plate with no central post support. The results obtained are very sensi

Figure 4.6. Velocity vector field and contour lines of temperature and pressure in Case 3B.

FIGURE 1. VELOCITY VECTOR FIELD
TIME = 1.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

FIGURE 2. VELOCITY VECTOR FIELD
TIME = 2.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

FIGURE 3. VELOCITY VECTOR FIELD
TIME = 3.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

FIGURE 4. VELOCITY VECTOR FIELD
TIME = 4.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

FIGURE 5. VELOCITY VECTOR FIELD
TIME = 5.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

FIGURE 6. TEMPERATURE CONTOUR LINES
TIME = 1.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

FIGURE 7. TEMPERATURE CONTOUR LINES
TIME = 2.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

FIGURE 8. TEMPERATURE CONTOUR LINES
TIME = 3.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

FIGURE 9. TEMPERATURE CONTOUR LINES
TIME = 4.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

FIGURE 10. TEMPERATURE CONTOUR LINES
TIME = 5.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

FIGURE 11. PRESSURE CONTOUR LINES
TIME = 1.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

FIGURE 12. PRESSURE CONTOUR LINES
TIME = 2.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

FIGURE 13. PRESSURE CONTOUR LINES
TIME = 3.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

FIGURE 14. PRESSURE CONTOUR LINES
TIME = 4.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

FIGURE 15. PRESSURE CONTOUR LINES
TIME = 5.00000E+01, X-AXIS = 0.00000E+00, Y-AXIS = 0.00000E+00

Figure 4.7. Velocity vector field and contour lines of temperature and pressure in Case 4B.

tive to the damping ratio. The predicted lifetime for the carbon steel plate (3' x 3' x 1/4") subjected to a vortex shedding noise (156.5 decibels, equivalent frequency $f_e = 43$ Hz), and a damping ratio $\xi = 0.0015$, was found to be of only 50 days. Lifetime prediction for the fully supported plate with a central post and Kaowool backing is presently evaluated. From the above results it is evident that a thermal liner section with a failed central post and a Kaowool insulation of degraded integrity (i.e. no resilience), would rapidly deteriorate and experience cover plate failure.

Failure of the cover plate and likely loss of Kaowool insulation (due to helium flow interaction) would cause, a 42" x 42" hot patch to develop on the thermal liner. The 3/4"-thick liner plate exposed to the helium gas would have a surface temperature profile close to the helium temperature (varying from 20°F to 100°F below the helium temperature taken at 750°F). Some scoping calculations indicate that the cooling water temperature in the main header will only rise by 0.6°F (on the basis of 10 tubes feeding into the header). This small temperature rise would be insufficient to be detected and to indicate the need for preventive measures.

This situation is being analyzed using a two-dimensional stress-strain model and the program SAP-V. On the assumption that all the cooling tubes and the anchor bolts remain in place and constrain the plate motion, thermal stresses in the range of 140,000 to 160,000 psi will develop and cause the liner plate to fail. A more conservative case in which the anchor bolts and the cooling tubes are sheared in the hot patch area is being analyzed and will be reported later.

The assembling of the flow-noise test facility is completed. A 22" x 22" x 1/16" aluminum plate (simulating the thermal insulation cover plate) has been instrumented (5 strain gauges, 5 LVDT) and mounted in the test section. Four vortex generators of 2 inches in diameter have been placed upstream of the test section. Flow noise signals from the microphones and strains and displacement signals are being received on a scope and a spectrum analyzer. The electronic instrumentation is being checked for accuracy and linearity between input and output. An excessive noise produced by the signal modulator of the strain gauges will be resolved by a modification of the present conditioner.

The measurement of the damping coefficient of a full size (42" x 42" x 1/4") cover plate is in progress. The damping coefficient of the plate with the four edge bolts and no central post nor Kaowool backing has been measured to be 0.0011. With one of the four bolts loosened this damping coefficient is increased to 0.0015. Measurements are in progress for the case with central post and Kaowool backing (with 30% compression).

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II. LMFBR SAFETY EVALUATION

SUMMARY

Under the LMFBR experimental program, work has begun this quarter on the hydrodynamic dispersion effort in both analytical and experimental areas. An analysis has been completed showing the importance of condensation rates on dispersal in closed boiling pools. Construction and checkout of the new boiling pool apparatus has been nearly completed. Testing for freezing and streaming of single-phase liquids has been virtually completed and preparation of a final report is in progress.

Significant progress during this reporting period for the SSC Code Development program was made in the following areas: (1) Transient flow redistribution in the reactor core, (2) Modeling of the heat transport system and (3) System integration method. The test model for interchannel flow distribution was applied to the end-of-life core conditions in the Clinch River Breeder Reactor Plant (CRBRP). It was found that this effect indeed lowers the maximum hot channel temperature of liquid sodium. This resulted from a drastically reduced bypass flow. A flow reversal in the bypass channel was also predicted. The heat transport system has been programmed, debugged and tested. Two test cases using different boundary conditions were run. A study for the number of nodes required was also made. Up to 4-ft long nodes could be used without any significant drift in computed results. The multi-step scheme (MSS) for time integration was adapted to a simplified test problem representing the reactor vessel and the primary heat transport system. This scheme was found to be faster by at least a factor of two when compared with the commonly used single-step method.

The Code Validation effort during this reporting period has concentrated on the design of the BIZET experiments to be performed on the UKAEA's ZEBRA critical facility and on methods of doing sensitivity analysis for SSC validation.

Under the FRS Reliability Assessment research program, two methods were employed for the approximate calculation of the first few moments of the reliability and/or availability of a system described by a Markov model with random transition probabilities. These methods are: (1) the Monte-Carlo simulation, and (2) the Taylor series expansion. Once obtained, the moments of reliability are used in calculating various uncertainty measures through the moment matching technique.

1. Fast Reactor Assessment - Accident Sequence Studies (O. C. Jones)

1.1 Hydrodynamic Characteristics of Two-Phase Dispersal Systems (T. Ginsberg)

A. Analytical Investigations

An analytical model is being developed to describe the flow dynamics in a two-phase pool with volumetric gas injection. The model will be used to guide the experimental system design, and will serve as a preliminary framework for future modeling effort.

Consider a two-phase pool with volumetric gas injection, shown in Fig. 1.1. The cross-sectional area is A , and the length is L . The volumetric gas source strength is $S_g(x)$. A range of flow regimes is expected to exist within the pool. As a first approximation, assume the existence of the following flow regimes (see Fig. 1.1).

(i) Bubbly ($x = 0$ to $x = x_b$)

This regime is characterized by widely separated, non-intersecting bubbles.

(ii) Churn-turbulent ($x = x_b$ to $x = x_c$)

This region is characterized by closely arranged, interacting bubbles.

(iii) Interface transition region ($x = x_c$ to $x = x_d$)

This region, which may be a well-defined surface or a highly transient oscillatory region, separates the dense from the dispersed region. Droplets may be torn away from the liquid pool in this region.

(iv) Dispersed region ($x = x_d$ to $x = L$)

This region contains droplets dispersed in the gas phase. A liquid film flows along the wall, and droplets impinge on the film. Droplets may be carried off with the gas flow, or may reflux back into the liquid pool.

The analytical model of the two-phase pool system is being designed to incorporate descriptions of the following phenomena:

(i) The void distributions in all of the flow regions described above.

(ii) The rate of droplet entrainment into the dispersed flow region.

(iii) The rate of droplet deposition on the liquid film.

(iv) The flow rate and thickness of the liquid film.

(v) The flow regime transition criteria.

B. Experimental Investigations

Design of the experimental system has been initiated. Determination of design criteria for the test section, gas delivery system, and instrumentation is in progress.

1.2 Dispersion and Heat Transfer in Two-Phase Boiling Systems (T. Ginsberg)

A. Analytical Investigations

The transition phase of loss-of-flow HCDA may be characterized by the existence of closed, boiling UO_2 -steel pools, bounded axially by frozen fuel-steel plugs. The flow regime during this phase of the accident may play a significant role in determining the probability of recriticality events (Fauske, 1975). Dhir (1976) has argued that the rated vaporization in adiabatic, closed UO_2 systems is two orders of magnitude lower than that required to sustain dispersed conditions. Farahat (1976), however, showed experimentally that boundary heat losses can significantly increase the vapor generation rate.

The analysis described below was developed in order to explain and rationalize the apparently divergent claims of Dhir (1976) and Farahat (1976), and to develop a relatively simple model of the behavior of closed boiling systems. The computation is actually of the vapor source term for a homogeneous, equilibrium, closed, adiabatic system with condensation heat losses.

Consider a homogeneous, closed, simple component equilibrium boiling system of fixed volume V .

The first law of thermodynamics is

$$\frac{d}{dt} (M_l h_l + M_v h_v) - V \frac{dP}{dt} = \dot{Q} - \dot{Q}_C - \dot{Q}_N \quad (1.1)$$

where M_l , M_v are the liquid and vapor masses, h_l , h_v are the liquid and vapor enthalpies, P is the system pressure, $\dot{Q}$ is the rate of internal generation, and $\dot{Q}_C$ and $\dot{Q}_N$ are the rates of condensation and non-condensation heat losses.

The system mass balance is

$$\frac{d}{dt} (M_v + M_l) = 0 \quad (1.2)$$

The Clausius-Clapyron equation is

$$\frac{dP}{dT} = \frac{h_{fg}}{v_{fg} T} \quad (1.3)$$

where T is the system temperature, h_{fg} the latent heat of vaporization, and v_{fg} the difference of vapor-liquid specific volumes.

The ideal gas law for the vapor phase, at low system pressure, is

$$\frac{1}{P} \frac{dP}{dt} - \frac{1}{M_v} \frac{dM_v}{dt} - \frac{1}{T} \frac{dT}{dt} = 0 \quad (1.4)$$

Equations (1.1)-(1.4) can be solved for dP/dT , dT/dt , dM_v/dt . The result for dM_v/dt is

$$\frac{dM_v}{dt} = (Q - Q_C - Q_N) \left[h_{fg} + \frac{M_2}{M_v} \frac{C_{p2} T v_{fg}}{h_{fg}} \left(\frac{1}{P} - \frac{v_{fg}}{h_{fg}} \right)^{-1} - \frac{V}{M_v} \left(\frac{1}{P} - \frac{v_{fg}}{h_{fg}} \right)^{-1} \right]^{-1} \quad (1.5)$$

The vapor balance equation for the system is

$$\frac{dM_v}{dt} = \dot{m}_g - \frac{\dot{Q}_C}{h_{fg}} \quad (1.6)$$

where the second term in Eq. (1.7) represents a mass sink due to vapor condensation at the system boundaries. The quantity $\dot{m}_g$, the rate of vapor production, can be obtained using Eqs. (1.5) and (1.6).

Equation (1.5) was analyzed for a UO_2 system near 1 atm. An order-of-

magnitude analysis revealed that to good approximation Eq. (1.5) can be represented by

$$\frac{dM_v}{dt} = (\dot{Q} - \dot{Q}_C - \dot{Q}_N) \left[\frac{M_\ell}{M_v} \frac{C_\ell T_{vfg}}{h_{fg}} \frac{1}{\left(\frac{1}{P} - \frac{v_{fg}}{h_{fg}} \right)^{-1}} \right] \quad (1.7)$$

Equations (1.6) and (1.7) were used to obtain the following simplified expression for the superficial vapor velocity j_g ($= \dot{m}_g / A \rho_g$), where ρ_g is the vapor density, and A is the pool cross-sectional area:

$$\frac{j_g}{j_g^*} = \frac{\dot{Q}_C}{\dot{Q}} + \left[\left(1 - \frac{\dot{Q}_C}{\dot{Q}} - \frac{\dot{Q}_N}{\dot{Q}} \right) \frac{\rho_g}{\rho_\ell} \left(\frac{h_{fg}}{C_\ell T} \right)^2 \frac{\bar{\alpha}}{1-\bar{\alpha}} \frac{C_\ell}{R} \right] \quad (1.8)$$

In Eq. (1.8), j_g^* is the superficial vapor velocity computed assuming all the energy generated is used to vaporize liquid, ρ_ℓ is the liquid density and $\bar{\alpha}$ the average void fraction.

Eq. (1.8) is plotted in Fig. (1.2) for a UO_2 system. Dhir's result (Dhir, 1976) for an adiabatic system is represented by $F_N = F_C = 0$, in which case only 0.55% of all the energy generated is used to vaporize fluid. At full-power conditions, with this vaporization rate, the flow regime map of Fauske (1975) predicts that the boiling UO_2 system is not dispersed. However, as Farahat (1976) showed and as shown in Fig. (1.2), the condensation vapor sink can potentially lead to significant vapor generation rates. Evaluation of boundary condensation rates during the transition phase accident sequences is of significant importance.

B. Experimental Investigations

Evaluation of the behavior of enclosed or partially enclosed boiling systems has begun preparatory to the design stages of this effort.

1.3 Boiling Pools with Internal Heat Generation (G. A. Greene, T. Ginsberg, C. E. Schwarz)

1.3.1 Boiling Pool Heat Transfer to Vertical Surfaces

This task was completed in the second quarter, FY 1977. A final report has been written and is being issued.

1.3.2 Boiling Pool Heat Transfer to Inclined Surfaces

a. Instrumentation Development. The construction of the inclinable wall boiling pool apparatus has been completed by the machine shop facility and is on hand (see Fig. 1.3). Minor structural modifications are currently being performed on the apparatus. The coolant channels through the base, walls, and electrodes of the apparatus have been manifolded and are operational. Construction and implementation of the coolant water and vaporization make-up water circuits is nearing completion. Problems associated with leakage from the electrode coolant, test section coolant, and structural and test wall members (see Figs 1.4, 1.5) have been identified and corrected.

The 0.010 inch diameter stainless steel-sheathed Chromel-Alumel thermocouple wire and accessories have been received. The in-house construction of the gold-plated thermocouples has begun. Two foot lengths of wire have been cut and are being polished flat on one end in preparation for gold-plating (see Fig 1.6, Greene, 1977).

The data acquisition and analysis system is nearing completion and operational capability is expected shortly. The HP2911 A,B Cross Bar Scanner, HP 3455 A Integrating Digital Voltmeter, HP 21 MX Computer, Statos 42 Printer-Plotter are all on hand and operational. Delivery of the computer interface card for the Cross Bar Scanner has arrived and interfacing will be completed shortly.

Competitive bids are being requested for electronic reference junction channels for the thermocouples. This procedure will be a major task milestone. A vertical axis traversing mechanism is being considered with motorized controller and LVDT output to obtain vertical temperature as well as pressure profiles in the boiling pool.

Software development for computerized on-line data acquisition, reduction analysis and graphical and tabular output is currently under way. Primary tasks have been block diagrammed; flow charts for the FORTRAN coding have been begun. Software routines should be completed and debugged in the third quarter, FY 1977.

b. Measurement Concept Evaluation. An experimental program was undertaken to evaluate the concept of cooling the rear of the test plate (Boron Nitride) by pool boiling to achieve an isothermal surface. If it is possible to insure a uniform back surface temperature and measure it accurately, the thermocouple instrumentation requirements would be reduced by approximately a factor of one-half.

The inclined-wall boiling pool prototype apparatus was utilized for this experiment. The apparatus was modified as in Fig. (1.7) An enclosed compartment was constructed such that it would have a wall in thermal

contact with the volumetric boiling pool. Commercial thermocouples were used to measure the surface temperature in contact with the nucleate pool boiling Freon-11. Five Chromel-Alumel thermocouples were machined flat in order that they be mounted flush with the pool boiling surface. The thermocouple lead wires were led through the wall and boiling pool to the measuring apparatus.

A Freon-11 coolant loop was assembled, consisting of a condenser heat exchanger, and pump. It was attempted to introduce Freon at a low flow rate and at a temperature close to the boiling temperature into the chamber and cool the wall in contact with the volumetric boiling pool by nucleate pool boiling. It has been shown by Rohsenow (1952) and others (Thom, 1965, and Collier, 1972) that under low flow rate and low subcooling conditions for nucleate boiling of water, the wall superheat temperature may be kept small and nearly constant, varying with wall heat flux to the one-fourth power,

$$\Delta T_{\text{SAT}} \sim \phi^{.25}.$$

It was hoped that this relationship being somewhat insensitive to small change in ϕ , the wall heat flux, that a uniform wall surface temperature could be achieved. Five experimental runs were attempted with uncalibrated thermocouples and the results are reported in Table (1.1) Thermocouple number one was reading extraordinarily high; examination revealed it had been pulled back from the surface and its reading has been disregarded. A typical spread in the surface temperature was 3 - 4° F. It is expected that with more controlled conditions (i.e., flow rate, subcooling, etc.), this range can be reduced to 1 - 2° F. Thus, it is presently being considered to measure the back wall temperature distribution of the boron nitride test plate with 4 - 5 thermocouples, which will significantly reduce the back-side instrumentation requirements.

Thermocouple Number	Surface Temperature, F°				
	Run 1	Run 2	Run 3	Run 4	Run 5
2	80	80	78	80	80
3	78	77	76	79	79
4	77	79	75	77	77
5	75	76	74	76	76

Table 1.1 Boiling Temperature, Freon-11 = 74.9° F.
at 1 Atmosphere Pressure

1.4 Streaming and Freezing of Core Debris in Channel Geometry (M. H. Chun and J. J. Barry)

Data acquisition of Wood's metal tests with the upgraded instrumentation (as described in earlier Quarterly Report) have been completed. In addition, data acquisition of paraffin wax test with the upgraded instrumentation is nearly completed. A complete summary of the Wood's metal tests data which include those that were reported in the previous Quarterly Report as well as additional data obtained during the present reporting period is shown in Table 1.2.

As may be noticed in Table 1.2, up to 6 runs were made under nominally the same conditions to examine the degree of scatter in the data for both plugging time and molten fluid displacement for a given test condition. The following statistical observations may be made from the test results shown in Table 1.2.

1. The relative standard deviation of the plugging time (or the time at which essentially all the available mass has been displaced) varies from 6% to 22%.

2. The relative standard deviation of the total mass displacement prior to plugging, on the other hand, was between 15% and 20% (excluding the cases where essentially all the available mass was displaced prior to plugging).

Comparisons of the plugging times and the mass displacements between the experimental data and model predictions (i.e., earlier computer solutions developed at BNL and the simplified model briefly described in the previous Quarterly Report) are shown in Figures 1.8 through 1.12. These figures show that the model under-predicts about 50% at lowest temperature level (i.e., initial Wood's metal temperature of 75° C. which is close to the freezing point of the Wood's metal) and the agreement improves at higher temperature levels.

Further detailed description of the simplified analytical model for freezing process along with the complete review of the experimental data will be included in a final report which is under preparation at present.

Test Conditions					Experimental Results							
Run No.		Initial Wood's Metal Temp. (°C)	Coolant Inlet Temp. (°C)	Liquid Head		Total Mass Displaced			Plugging Time			
				Initial (cm)	Final (cm)	Total Mass Displaced (g)	Mean Value (g)	Standard Deviation (g)	Plugging Time (Sec.) (Absolute)	Adjusted Plugging Time (sec)	Mean Value (sec)	Standard Deviation (sec)
3	E	105	14.4	13.6	3.9	12,444.6			18.2	17.9		
4	A	125	14.7	13.6	0.3	19,827.2	19,431.4	588.4	24.0*	23.7*	18.8*	4.2
	B		14.4		0.7	19,048.6			17.3*	17.0*		
	C		15.0		0.8	19,622.0			23.9*	23.6*		
	D		15.6		0.2	19,828.3			13.0*	12.7*		
	E		14.4		0.0	19,857.1			18.6*	18.3*		
	F		14.7		0.5	18,405.3			17.6*	17.3*		
5	A	135	15.6	13.6	0.7	19,578.3	19,489.5	337.5	13.7*	13.4*	15.0*	2.3
	B		14.4		0.5	19,656.8			17.7*	17.4*		
	C		15.6		0.5	19,715.7			15.6*	15.3*		
	D		13.9		0.3	19,603.7			17.1*	16.8*		
	E		15.0		0.5	18,893.2			12.3*	12.0*		

Reservoir Pressure = 1 psig (68947.6 dynes/cm²); Test Section I.D. = 1.11 cm.
Coolant Flow Rate = 13.24 liters/min

- Note: 1. * Time at which essentially all the available mass has been displaced.
2. Initial and final heads shown in Table 1 are liquid levels above the inlet of the test section.

Table 1.2 (Cont'd) Test Conditions and Experimental Results (Wood's Metal)

Test Conditions						Experimental Results						
Run No.		Initial Wood's Metal Temp. (°C)	Coolant Inlet Temp. (°C)	Liquid Head		Total Mass Displaced			Plugging Time			
				Initial (cm)	Final (cm)	Total Mass Displaced (g)	Mean Value (g)	Standard Deviation (g)	Plugging Time (Sec.) (Absolute)	Adjusted Plugging Time (sec)	Mean Value (sec)	Standard Deviation (sec)
3	E	105	14.4	13.6	3.9	12,444.6			18.2	17.9		
4	A	125	14.7	13.6	0.3	19,827.2	19,431.4	588.4	24.0*	23.7*	18.8*	4.2
	B		14.4		0.7	19,048.6			17.3*	17.0*		
	C		15.0		0.8	19,622.0			23.9*	23.6*		
	D		15.6		0.2	19,828.3			13.0*	12.7*		
	E		14.4		0.0	19,857.1			18.6*	18.3*		
	F		14.7		0.5	18,405.3			17.6*	17.3*		
5	A	135	15.6	13.6	0.7	19,578.3	19,489.5	337.5	13.7*	13.4*	15.0*	2.3
	B		14.4		0.5	19,656.8			17.7*	17.4*		
	C		15.6		0.5	19,715.7			15.6*	15.3*		
	D		13.9		0.3	19,603.7			17.1*	16.8*		
	E		15.0		0.5	18,893.2			12.3*	12.0*		

Reservoir Pressure = 1 psig (68947.6 dynes/cm²); Test Section I.D. = 1.11 cm
Coolant Flow Rate = 13.24 liters/min

- Note: 1. * Time at which essentially all the available mass has been displaced.
2. Initial and final heads shown in Table 1 are liquid levels above the inlet of the test sections

Table 1.2 (Cont'd) Test Conditions and Experimental Results (Wood's Metal)

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ROHSENOW, W.M., "A Method of Correlating Heat Transfer Data for Surface Boiling of Liquids," Trans. ASME, 74, 969 (1952).

THOM, J. R. S., et al., "Boiling in Subcooled Water During Flow up Heated Tubes or Annuli," Symposium on Boiling Heat Transfer in Steam Generating Units and Heat Exchanges, Manchester, England, September, 1965.

Figure 1.1 Schematic of the two-phase pool with gas injection.

Figure 1.2 Superficial Velocity for closed, boiling UO_2 System

Figure 1.3 Schematic Diagram of the Boiling Pool

Figure 1.4 Side view photograph of the boiling pool

Figure 1.5 Top view photograph of the boiling pool

Figure 1.6 Schematic diagram of thermocouple fabrication

Figure 1.7 Modification of boiling pool mockup for cooling surface tests with Freon

Figure 1.8 Reproducibility and comparison with Theory for Wood's Metal Inlet Temperature of 75°C

Figure 1.9 Reproducibility and Comparison with Theory for Wood's Metal Inlet Temperature of 90°C

Figure 1.10 Reproducibility and Comparison with Theory for Wood's Metal Inlet Temperature of 105°C

Figure 1.11 Reproducibility and Comparison with Theory for Wood's Metal Inlet Temperature of 125°C

Figure 1.12 Reproducibility and Comparison with Theory for Wood's Metal Inlet Temperature of 135°C

2. Technical Coordination of Structural Integrity (M. Reich)

A report containing a compilation of references, data sources and analysis methods for LMFBR primary piping system components BNL-NUREG 50650 (NRC-7) was completed and sent to RSR on March 15. This is the first of a series of reports, the aim of which is to provide a critical review of the theories of fracture and the application of fracture mechanics methods to life prediction, reliability and safety analysis of piping components in nuclear plants undergoing subcreep and elevated temperature service conditions. BNL-NUREG 50650 provides the background material for the review of fracture based failure criteria as used in industrial application or as proposed for future developments. The potentially applicable literature which contains over 350 references has been grouped in the following somewhat overlapping areas:

1. Linear - elastic fracture theory.
2. Elastic - plastic fracture theory and ductile failure.
3. Multiaxial conditions, toughness testing.
4. Fatigue.
5. Time-dependent crack propagation (corrosion, creep-fatigue, creep).
6. Life prediction.

3. SSC Code Development (A. K. Agrawal)

The Super System Transient (SSC) program deals with the development of an advanced thermohydraulic system transient code for LMFBRs. Currently, a version of this program (SSC-L) is being developed for the safety analysis of loop-type plants. This version of the code will be capable of simulating safety and design transients. Reference is made to the previous quarterly progress report (Agrawal, 1977) for a summary of accomplishments prior to the start of the current period.

3.1 Transient Single-Phase Coolant Dynamics (J. W. Yang)

Transient single-phase coolant dynamics models for the entire vessel were tested together by coupling the boundary conditions between pertinent components of the vessel. The major components are: mixing in lower inlet plenum, flow redistribution in core and bypass channel, and mixing in upper outlet plenum. A test case based on the Clinch River Breeder Reactor Project (CRBRP) design data was prepared. The entire core was modeled by ten channels. The selection of channels was made according to the relative radial power and orifice zone as shown in Table 3.1 and Fig. 3.1. The designation of channels thus selected is shown in Fig. 3.2. Two boundary conditions representing the coupling between vessel and primary loops were imposed for the test case. The conditions are (1) the normalized flow rate and (2) coolant temperature at the inlet of the vessel. In addition, a normalized heat flux to coolant was used in the test case to replace the coupling between the coolant and fuel rod. These conditions are shown in Fig. 3.3.

In the lower inlet plenum, both sodium and metal temperatures were computed according to the lumped-parameter ideal mixing model. The results are given in Fig. 3.4 and indicate a relatively small temperature change for both sodium and metal due to the large storage of sodium and high mass heat capacity of metal. Thus, the variation of vessel inlet temperature is not reflected in the lower plenum, and the coolant temperature at the entrance of core is relatively stable.

In the extended core region, flow redistribution is computed as discussed in the previous quarterly progress report (Agrawal, 1977). For the test case, the bypass channel has the lowest heating rate (the power fraction at steady state is only 0.6%). Consequently, the bypass flow is drawn into the core due to the buoyancy forces generated by the relatively high heating rates in the core. Transient flow rates are shown in Fig. 3.5 for two fuel channels and the bypass channel. It is seen that the bypass flow rate is reduced rapidly for the first 35 seconds. This rapid reduction of flow rate causes flow reversal in the bypass channel. However, the flow is recovered gradually and eventually becomes stagnant. The reduction of bypass flow provides additional flow into the core region. An increase of flow rate is seen for all of the ten channels. The rate of flow increase in each channel varies according to its buoyancy force and hydraulic resistance as described by Agrawal (1976). A detailed comparison is given in Table 3.2. The redistribution of flow has an important effect on coolant temperature. Table 3.3 provides a comparison of the maximum coolant temperatures. It is seen that flow redistribution tends to flatten the core-wide radial temperature distribution. The transient coolant temperature for two channels is shown in Fig. 3.6.

TABLE 3.1

Selection of Reactor Core Channels for the Test Case

Channel Number	Sub-Assembly Number	Orifice Zone	Number of Assemblies	Relative Radial Power	Average Radial Power	Fraction of Total Power	Fraction of Total Flow
1	1	1	6	1.215	1.2315	0.1326	0.1067
	7	1	6	1.181			
	18	1	6	1.265			
	24	1	6	1.265			
2	3	1	6	1.078	1.0845	0.1164	0.1067
	6	1	6	1.095			
	19	1	6	1.106			
	22	1	6	1.059			
3	2	1	6	0.985	0.9703	0.0782	0.080
	20	1	6	0.943			
	23	1	6	0.985			
4	4	2	6	1.146	1.1018	0.1183	0.0958
	11	2	6	1.105			
	14	2	6	1.105			
	8	2	6	1.051			
5	16	2	6	1.000	1.0420	0.0561	0.0478
	68	2	6	1.084			
6	10	2	6	0.958	0.9593	0.1030	0.0958
	15	2	6	0.955			
	5	2	6	0.968			
	25	2	6	0.956			
7	13	2	6	0.910	0.9030	0.0727	0.0718
	26	2	6	0.944			
	32	2	6	0.855			
8	12	3	6	0.877	0.9172	0.1477	0.1352
	17	3	6	0.851			
	27	3	6	0.822			
	31	3	6	0.989			
	9	3	6	1.007			
	21	3	6	0.957			
9	28	4	6	0.911	0.8013	0.0645	0.0602
	30	4	6	0.836			
	29	5	6	0.657			
10	RBZ*	---	150	-----	-----	0.1040	0.12
Bypass	----	---	---	-----	-----	0.0069	0.08

*RBZ = Radial Blanket Zone.

GO

Fig. 3.2. Coolant channel designation used in the test case.

Fig. 3.3. Assumed boundary conditions used in the test case.

Fig. 3.4. Predicted transient response in lower inlet plenum.

Fig. 3.5. Predicted transient flow rates in core with flow redistribution model.

TABLE 3.2

Effect of Flow Redistribution on Fraction of Flow

Channel	Orifice Zone	Steady-State Average Power Values kW/ft	Fraction of Flow at 120s		Change %
			Without Redistribution	With Flow Redistribution	
1	1	8.28	0.1067	0.1216	14.0
2	1	7.29	0.1067	0.1150	7.8
3	1	6.53	0.080	0.0819	2.3
4	2	7.50	0.0958	0.1114	16.3
5	2	7.00	0.0478	0.0539	12.8
6	2	6.45	0.0958	0.1043	12.8
7	2	6.07	0.0718	0.0753	4.9
8	3	6.17	0.1352	0.1482	9.6
9	4.5	5.39	0.0602	0.0661	9.8
10	RBZ*	2.46	0.12	0.1222	1.8
Bypass	----	----	0.08	0	----

* RBZ = Radial Blanket Zone.

TABLE 3.3

Effect of Flow Redistribution on Maximum Coolant Temperature

Channel	Steady-State Average Power Values kW/ft	Coolant Temperature at 120s in K		$\Delta T, K$ ($T_{Yes} - T_{No}$)
		Flow Redistribution Yes	No	
1	8.28	967.6	1013.0	-45.4
2	7.29	945.3	968.8	-23.5
3	6.53	926.8	933.7	- 6.9
4	7.50	963.9	1016.1	-52.2
5	7.00	951.0	990.4	-39.4
6	6.45	937.5	963.8	-26.3
7	6.07	930.4	944.5	-14.1
8	6.17	940.5	969.2	-28.7
9	5.39	933.5	961.9	-28.4
10	2.46	896.7	901.4	- 4.7

Maximum Radial Temperature Difference

$$T_1 - T_{10} = 70.9 \quad \text{With Flow Redistribution}$$

$$T_4 - T_{10} = 114.7 \quad \text{No Flow Redistribution}$$

Fig. 3.6. Predicted maximum coolant temperature with and without flow redistribution.

In the upper plenum, temperatures of the various components are computed according to the mixing model. Results are shown in Fig. 3.7. For the test case, stratification occurs only for a short period between 10 to 25s. During this period, the two-zone mixing model is applied which yields two temperatures for sodium.

It is concluded that the various models of the single-phase flow are coupled successfully in the test case.

3.2 Sodium Boiling and Fission Gas Release (V. Quan)

A model for fission gas release and sodium boiling was described in the last quarterly progress report (Agrawal, 1977). During the present reporting period, a stand-alone program was written to test it. Work is continuing on running test cases.

3.3 Heat Transport System (I. K. Madni)

3.3.1 System Hydraulics

The pump model described in the earlier quarterly report (Agrawal, 1977) was tested successfully for coastdown in the primary loop with scram operative, and sample results were obtained. These are shown in Figs. 3.8 and 3.9.

As a follow-up to this work, a hydraulic model has been developed for the primary heat transport system, in order to predict the response of the system to a pipe rupture in one of its loops with scram operative and all primary pumps coasting down. As a first step in the analysis, the break was chosen close to the vessel inlet as this is believed to result in the most severe hydraulic transient. The system configuration for the test case is illustrated in Fig. 3.10. This is a two-loop simulation, with the damaged loop being treated as one loop and all intact loops as the other loop.

The model equations are described below.

Momentum: The uniform mass flow rate model implies that at any instant in time, flow rates are constant throughout the loops except at a free surface, or junction where several flows meet, or a break where there is flow loss. With this as background, the flow rates have been indicated in Fig. 3.10. Volume-averaged momentum equations can be written relating rate of change of mass flow rate in a constant mass flow rate section to the terminal pressures and losses in that section. These are

$$\frac{dW_{1,1}}{dt} \sum_{1,1} \frac{L}{A} = P_1 - P_{in,1} - (\Delta P_{f,g})_{1,1} \quad (3.1)$$

$$\frac{dW_{2,1}}{dt} \sum_{2,1} \frac{L}{A} = P_{out,1} - P_b - (\Delta P_{f,g})_{2,1} \quad (3.2)$$

$$\frac{dW_{3,1}}{dt} \sum_{3,1} \frac{L}{A} = P_b - P_{inV} - (\Delta P_{f,g})_{3,1} \quad (3.3)$$

Fig. 3.7. Predicted transient thermal response in upper plenum.

Fig. 3.8. Pump coastdown with scram (test case).

Fig. 3.9. Pump coastdown with scram (test case).

Fig. 3.10. Hydraulic profile of PHTS for test case.

Fig. 3.11. Nodal diagram for flow in piping.

$$\frac{dW_{1,2}}{dt} \sum_{1,2} \frac{L}{A} = P_1 - P_{in,2} - (\Delta P_{f,g})_{1,2} \quad (3.4)$$

$$\frac{dW_{2,2}}{dt} \sum_{2,2} \frac{L}{A} = P_{out,2} - P_{inV} - (\Delta P_{f,g})_{2,2} \quad (3.5)$$

Pump: The pressure in the loop at pump exit is obtained from

$$P_{out,k} = P_{in,k} + \rho g H(k) \quad k = 1,2 \quad (3.6)$$

where $H(k)$ are pump heads determined from the pump model described earlier.

Reservoir: The inlet pressure to the pump is governed by the behavior of coolant and cover gas in the pump tank, as follows:

$$P_{in,k} = P_{gas,k} + \rho g H_{1,k} + K_{res} (W_{1,k} - W_{2,k}) | (W_{1,k} - W_{2,k}) | \quad k = 1,2 \quad (3.7)$$

where

$$P_{gas,k} = \frac{m_{gas} RT_{gas}}{A_{res} (H_{tot} - H_{1,k})} \quad (3.8)$$

and $H_{1,k}$ is obtained from

$$A_{res} \frac{d}{dt} (\rho H_{1,k}) = W_{1,k} - W_{2,k} \quad (3.9)$$

Here, the cover gas volume is assumed isolated and governed by the perfect gas law.

Reactor vessel: The level of sodium in the upper plenum is determined by

$$A_{RV} \frac{d}{dt} (\rho H_{RV}) = W_c - W_{1,1} - 2W_{1,2} \quad (3.10)$$

where

$$W_c = W_{3,1} + 2W_{2,2} \quad (3.11)$$

P_1 , the pressure at inlet to the loop, is given by

$$P_1 = P_{gas,RV} + \rho g H_{RV} \quad (3.12)$$

and $P_{gas,RV}$ is determined as in Eq. (3.8). An algebraic equation for P_{inV} is formulated, based on the relative resistances of the core and loops, as follows:

$$P_{inV} = (A + B + C) / \left(\sum_{2,2} \frac{L}{A} + \sum_{3,1} \frac{L}{A} + \sum_C \frac{L}{A} \right) \quad (3.13)$$

where

$$A = 2 \left[P_{\text{out},2} - (\Delta P_{f,g})_{2,2} \right] / \sum_{2,2} \frac{L}{A} \quad (3.14)$$

$$B = \left[P_b - (\Delta P_{f,g})_{3,1} \right] / \sum_{3,1} \frac{L}{A} \quad (3.15)$$

$$C = \left[P_1 + (\Delta P_{f,g})_C \right] / \sum_C \frac{L}{A} \quad (3.16)$$

Break: P_b , the pressure at the break location, is obtained from

$$P_b = P_{\text{atm}} + \frac{1}{2\rho g} \left(\frac{W_{2,1} - W_{3,1}}{C_D A_{bk}} \right)^2 \quad (3.17)$$

When this stand-alone module is interfaced with the rest of the heat transport system, the more detailed break model will replace Eq. (3.17), as also the reactor vessel equations will be replaced by a more detailed vessel hydraulic model.

Equations (3.1)-(3.17) form a system of ordinary differential equations and algebraic equations to be solved together to yield the flow rates, pressures, and free surface levels in the system. These equations are being solved using a predictor-corrector integration package, and sample results are being obtained.

3.3.2 Pump Characteristics

In the previous quarterly report (Agrawal, 1977), it was mentioned that under severe transients such as a massive pipe rupture, the pump could go through several regimes of operation, and accordingly, relations would be needed to describe pump behavior under those conditions also.

Complete characteristics have now been provided for the pump in terms of curve-fitted polynomial relations of head and torque as a function of pump speed and flow rate under different regions of operation. These relations have been developed from tabular values for a centrifugal pump with specific speed $n_s = 1800$ gpm units (Streeter, 1967). This is well within the range of specific speeds expected for loop-type LMFBR applications.

3.3.3 Piping

A detailed thermal model with discrete parameter representation has been formulated for the piping in the heat transport system. Figure 3.11 shows the model configuration.

The governing energy equations for coolant and wall (assuming perfect insulation on outer surface of wall) are as follows:

$$\text{coolant: } \rho A \Delta x \frac{de_{i+1}}{dt} = W \left[e_i - e_{i+1} \right] - U_{cw} A_{cw} \left[\frac{T_i + T_{i+1}}{2} - T_{w1} \right] \quad (3.18)$$

$$\text{wall: } M_w C_{w_i} \frac{dT_{w_i}}{dt} = U_{cw} A_{cw} \left[\frac{T_i + T_{i+1}}{2} - T_{w_i} \right] \quad \text{for } i=1, N-1 \quad (3.19)$$

where ρ , U_{cw} are evaluated at the midpoint between i and $i+1$.

A mixed implicit time-integration scheme is applied to Eq. (3.18), and the wall heat flux is allowed to be determined explicitly. (This scheme is similar to that employed in the core.) With this, Eq. (3.18) becomes

$$\begin{aligned} \rho^k A \Delta x \frac{(e_{i+1}^{k+1} - e_{i+1}^k)}{h} = & \frac{W^{k+1} + W^k}{2} \left[\frac{1}{2} (e_i^{k+1} - e_{i+1}^{k+1}) + \frac{1}{2} (e_i^k - e_{i+1}^k) \right] \\ & - U_{cw}^k A_{cw} \left[\frac{T_i^k + T_{i+1}^k}{2} - T_{w_i}^k \right] \end{aligned} \quad (3.20)$$

and Eq. (3.19) becomes

$$M_w C_{w_i}^k \frac{(T_{w_i}^{k+1} - T_{w_i}^k)}{h} = U_{cw}^k A_{cw} \left[\frac{T_i^k + T_{i+1}^k}{2} - T_{w_i}^k \right] \quad (3.21)$$

The flow rate W^{k+1} in Eq. (3.20) is known since hydraulic calculations precede thermal calculations. Equations (3.20) and (3.21) are now uncoupled and the unknowns e_{i+1}^{k+1} and $T_{w_i}^{k+1}$ can be determined algebraically in a marching fashion going from $i=1$ to $N-1$.

A module based on the above was programmed and two test cases were run, one of which will be shown here. For this, the following set of boundary conditions were used:

- 28-inch OD x 27.5-inch ID x 150-ft long pipe
- Down ramp on sodium inlet temperature from 1000 to 800°F, from 0 to 10 sec.
- Down ramp on coolant flow rate from 1600 to 160 lb/sec., from 2 to 27 sec.

These numbers were converted to SI units, before being input to the calculational module. The number of axial nodes was varied from 3 to 160, and the results in terms of coolant temperature at pipe outlet as a function of time (also in SI units) are shown in Fig. 3.12. For this test case, $N=40$ appears adequate, with maximum deviation in results being less than 4K. The input signal is also shown as a function of time. Such severe temperature transients are not expected to occur within the scope of the SSC, hence, we may not need 40 nodes for the model to be adequate.

Fig. 3.12. Coolant outlet temperature vs time (test case).

Computation is extremely fast, with 360 sec. of transient time requiring less than 0.6 sec. execution time on the CDC 7600 machine for a 40 node representation.

3.4 Steam Generating System (W. L. Weaver III)

The work during this quarter can be divided into two categories, the first being continued efforts at developing correlations for needed thermodynamic properties. The second category is work directed toward the transient modeling of the steam generating system.

3.4.1 Thermodynamic Properties

The effort to develop correlations for the thermodynamic derivatives $(\partial p / \partial u)_v$ and $(\partial p / \partial v)_u$ was continued. During the last quarter, a correlation for C_v , the specific heat at constant volume, was developed for values of pressure and enthalpy spanning the two-phase region for pressures between 100 psia and the critical point. This correlation for C_v is used to find the two thermodynamic derivatives in the saturation region.

Correlations were developed during this quarter for the two thermodynamic derivatives in the superheated vapor region for pressures between 100 psia and the critical pressure and for enthalpies from the saturated vapor enthalpy at any pressure up to the enthalpy corresponding to a temperature of 1472°K at that pressure. The correlation for $(\partial p / \partial u)_v$ is accurate to $\pm 1\%$ everywhere, and the correlation for $(\partial p / \partial v)_u$ is accurate to $\pm 2.5\%$ almost everywhere.

In the course of development of these new correlations, it was found that the correlation for the specific volume of superheated steam previously used (Wulff 1976, Agrawal 1976) was not accurate enough for evaluation of the two thermodynamic derivatives. Subsequently, a new correlation for the specific volume in the superheated vapor region was also developed.

3.4.2 Transient Modeling

After a review of the types of transients to be simulated by the initial version of the steam generator model of the SSC-L code, the single-mass velocity flow model originally proposed (Agrawal, 1975) for the steam generator was discarded. Transients to be analyzed by the initial version of the SSC code are initiated by accidents in the reactor core, the primary heat transport or intermediate heat transport system. Their effect on the steam generating system is to change the amount of heat transported to the steam generators. This results in changes in the surface heat fluxes in the heat exchangers. Flow transients initiated by changes in the surface heat fluxes are classified as intermediate speed transients for which the momentum integral flow model is appropriate (Meyer 1961, Meyer 1962).

The momentum integral model is characterized by the assumption that the fluid properties may be evaluated at a reference pressure rather than at the local pressure. This assumption removes sonic effects associated with the fluid compressibility and the restriction placed on the maximum time step which can be used in the integration of the conservation equations is relaxed. An informal report describing the particular form of the momentum integral to be used for the steam generating system has been prepared and is under internal review.

The steady-state model of the steam generating system was modified to conform to the momentum integral model. These changes were incorporated into the steady-state portion of the SSC-L code. The steady-state solutions for the steam generating system were successfully computed using the new flow model. The effect of the change of flow model on the computed steady-state solution was very small.

3.5 System Integration Method (J. G. Guppy)

A major effort was begun this quarter to develop the scheme for interfacing the various system components for the transient version of SSC-L. As detailed in the last quarterly progress report (Agrawal, 1977), the multi-step scheme (MSS) for time integration will be used for SSC. This means that different components/processes will be advanced in time at different characteristic time steps (h).

The two main features to be investigated in the MSS method are (1) the logic for running various components at different h's and (2) the proper matching of the boundary conditions at the interfaces.

In order to demonstrate the feasibility and advantage of this MSS method, a test case (see Fig. 3.13) consisting of a simplified system model of reactor, inlet and outlet mixing plena, and intermediate heat exchanger (IHX) was studied. Nine ordinary differential equations (ODE) were required to simulate the time rate of change of POWER, TFUEL, TRIN, TROUT, TVOUT, T1OUT, T2OUT, W1 and W2. T2IN was assumed known. Interfacing of boundary conditions is required for TVIN and T1IN. For this model, no piping time delays were assumed, and all properties/correlations were assumed constant or evaluated at the previous time step.

In this case, four individual time steps, h_1 for solving IHX thermal equations, h_2 for the reactor coolant thermal equations, h_3 for the power and fuel temperature equations and h_4 for the hydraulic equations, were employed. In selecting the time step hierarchy, knowledge of the system response was used. Specifically,

- (1) Fuel response faster than reactor coolant response.
- (2) Reactor coolant response faster than loop response.

The resulting time step hierarchy was assumed to be

$$h_3 \leq h_2 \leq h_1$$

and

$$h_4 \leq h_1$$

Within the framework of this hierarchy, the individual time steps were allowed to vary as dictated by the rapidity of the transient and desired accuracy tolerances. The hydraulics equations were handled separately since they are only loosely coupled to the thermal equations through property relationships. They were advanced in time first, using temperature values from the previously completed time step.

Fig. 3.13. Model schematic.

To advance the system in time from $t \rightarrow t + h_3$, the following was done:

- (1) Advance W1 and W2 by h_4 , storing new values.
- (2) If $t + n_4 h_4 < t + h_1$, go to (1).
- (3) Advance POWER and TFUEL by h_3 (extrapolation of TRIN and TROUT required).
- (4) If $t + n_3 h_3 < t + h_2$, go to (3).
- (5) Advance TRIN, TROUT and TVOUT by h_2 (extrapolation of TVIN required).
- (6) If $t + n_2 h_2 < t + h_1$, go to (3).
- (7) Advance T1OUT and T2OUT by h_1 .

Whenever needed in steps (5) and (7), the previously determined (from step (1)) values of W1 and W2 are used.

The selection of the allowed time step is accomplished such that all variables under the jurisdiction of that time step may only change by some relative fraction (ϵ_i) during the time step h_i . For example, for TFUEL during h_3 :

$$\frac{\text{TFUEL}(t + h_3) - \text{TFUEL}(t)}{\text{TFUEL}(t)} \leq \epsilon_3 \quad (3.22)$$

Thus, the ϵ_i 's are relative accuracy criteria. A value of $\epsilon_1 = 0.001$ (i.e., 0.1% relative) was found to be adequate.

For the sample transient studied (normal plant scram), the overall results obtained by the MSS method were in excellent agreement to those where the same model was tested (using a sophisticated predictor-corrector integration package) in which all equations were advanced at a common time step. The results for this test case showed that a savings in computing time of a factor of two was realized using the MSS approach over the single-step approach. This savings is expected to improve even more when applied to the entire SSC transient package.

It should be noted that in the worst case, i.e., when system dynamics is such that the system integration has to be carried on for the smallest h , the MSS method automatically reduces to SSS, i.e., to integration of all equations at a common time step.

3.6 Code Development (J. G. Guppy, E. Cazzoli, R. J. Kennett, H. Makowitz and E. S. Srinivasan)

The work involved in implementing the variable dimensioning by offset (VD/O) scheme discussed in the previous quarterly (Agrawal, 1977), to the steady-state SSC-L package has been completed during this quarter.

Programming of various models for transient was also begun.

PUBLICATIONS

The following is a list of publications during the current reporting period from this activity:

AGRAWAL, A. K., GUPPY, J. G., MADNI, I. K., QUAN, V., WEAVER, W. L. III and YANG, J. W., "Transient Simulation of LMFBR Systems," Presented at the Mathematics and Computation Division Topical Meeting on Improved Methods for Analysis of Nuclear Systems, Tucson, Arizona, March 28-30, 1977; Nucl. Sci. Eng. (in press).

YANG, J. W., "A Note on Turbulent-Laminar Transition for Flow in Triangular Rod Bundles," Nucl. Sci. Eng. 62, 579 (1977).

YANG, J. W., "Penetration of Turbulent Jet with Negative Buoyancy in Upper Plenum of an LMFBR," Nucl. Eng. & Design 40, 297 (1977).

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AGRAWAL, A. K. et al., (1976), "Preaccident Modeling of an LMFBR Plant for SSC-L," Brookhaven National Laboratory, BNL-NUREG-50602 (December 1976).

AGRAWAL, A. K. et al., (1975), "SSC: An Advanced Thermohydraulic Transient Code for LMFBRs," Quarterly Progress Report, July-September 1975, Brookhaven National Laboratory, BNL-50467 (October 1975).

MEYER, J. E. and WILLIAMS, J. S. Jr., (1962), "A Momentum Integral Model for the Treatment of Transient Fluid Flow," Bettis Technical Reviews, WAPD-BT-25, p. 47 (May 1962).

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WULFF, W. et al., (1976), "Development of a Computer Code for Thermal Hydraulics of Reactors (THOR)," Fifth Quarterly Progress Report, October-December 1975, Brookhaven National Laboratory, BNL-NUREG-50534 (July 1976).

4. Code Validation (A. K. Agrawal & C. Durston)

The code validation work this quarter covers the following two areas:

- 4.1 Sensitivity Analysis Methods
- 4.2 Critical Experiment Analysis

The Sensitivity Analysis work covers work on the use of statistical methods for code validation, and the Critical Experiment Analysis covers work on the final planning stage of the RSR experiments in the BIZET series on the UKAEA-Winfrith critical facility ZEBRA.

4.1 Sensitivity Analysis Methods (T. Krieger and C. Durston)

Within the scope of Sensitivity Analysis, Brookhaven National Laboratory (BNL) has been looking at one aspect of experimental validation of computer codes namely, numerical experimental validation. This is fairly straightforward with small codes with short running times. However, large codes with a multitude of inputs and with long running times present cost problems which inhibit complete numerical verification. Due to this cost restraint primarily, and the sheer logistics of the process as well, it is important to plan the runs that one makes with the code to maximize the information return if one wants more than superficial verification.

The ideas we have been investigating, therefore, must necessarily be statistical in nature and deal with mathematically incompletely defined systems. They will be useful in the areas of sensitivity analysis and in the normal process of code verification. The objective in both these areas is to define how one regulates the input to the code to maximize the information available to the validator.

The previous quarterly (Agrawal 1977) outlined the methods in use in the water reactor code validation effort. These methods together with the computer codes acquired from H. C. Huang have provided BNL with a basic framework from which to investigate the feasibility of this statistical concept. This quarter BNL has initiated numerical experiments with the objective of resolving the following questions:

- (i) What information can be extracted from a small number of experiments (i.e. computer runs)?
- (ii) What experiments should be performed to maximize the information return?
- (iii) How sensitive is this information return to the number of input variables?
- (iv) What order designs should be used?

A literature search uncovered two candidates for investigation. These were the Random Balance method explained by Budne (1959) and The Multistage Group Screening method propounded by M. S. Patel (1962). Effective use of the group screening technique requires a knowledge of the signs of the sensitivities, thus it seemed profitable to combine the two methods. Numerical experiments on a problem with 100 variables (20 runs; 2 levels) have indicated that roughly 2/3 of signs of the sensitivities could be predicted correctly. Thus, using these signs

from the random balance run in a multi-stage group screening method improved the effectiveness of the method. Another improvement in the group screening method was to use the estimates of the sensitivities produced by the sparse random balance run. These were used to block the variables into groups of similar magnitudes. The effect of this is to enhance the chance of identifying important variables that have been obscured in the random balance prelude. The number of runs required for this method is high in order for the method to be practical. Thus, subsequent work has concentrated on methods of making the random balance method more effective. This work is still in progress. The first objective being to define the limitations of the basic method. The whole crux of the issue is the design of the input matrix. The efficiency of a particular design being tied to the rank of the sub-matrices.

4.2 Critical Experiment Analysis

4.2.1 BIZET Safety Related Experiments (H. Ludewig)

The series of safety related experiments proposed for the BIZET-B core were outlined in the previous quarterly report (Agrawal 1977). In this report, estimates of the reactivity change due to the proposed maximum-sized disruption are made. Previous estimates of this change were based on a core composition for a 1000 MWe reactor design. In these calculations, homogenized number densities and dimensions obtained from the UKAEA-Winfrith were used.

This analysis was based on ENDF/B-IV processed through MC²-II, and the final reactor calculations were carried out using two-dimensional transport theory (TWOTRAN). This procedure is outlined schematically in Fig. 4.1. MC²-II was used to create a 30-group macroscopic cross-section library which is characteristic of an outer zone. These zone libraries are combined in a one-dimensional calculation in which 16-group macroscopic libraries are created for the reactor calculations. In the problem under consideration, three macroscopic libraries were created. These libraries were characteristic of the inner core, radial blanket and the compacted zone. In this way, libraries useful for characterizing the core (plutonium, uranium and sodium), the blankets (uranium and sodium) and the compacted zone (plutonium and uranium) were created.

The sixteen broad group libraries were created by carrying out two one-dimensional and one two-dimensional calculations. The one-dimensional calculations considered a radial slice through the reactor midplane which included the inner core, outer core, radial blanket and reflector. The second one-dimensional slice was axial which included the inner core, axial blanket and reflector. Finally, the two-dimensional calculation considered the compacted zone, under a raided zone, both being surrounded by inner core material. These configurations are shown in Figs. 4.2 through 4.4.

Using the above procedure, it is felt that the spectral shifts implied by the configuration are approximately accounted for, within the confines of the methods currently available at BNL. Improvements in this procedure can only be made by carrying out two-dimensional, adjoining cell calculations with a suitably modified SDX code. Such a modified SDX code would also carry out the resonance calculation correctly. This step would fit in between the MC²-II calculation and the transport theory calculation in Fig. 4.1.

The first two calculations carried out using this technique were a base case and one with a centrally located compacted zone. In the compacted zone, a doubling

Fig. 4.1 - Schematic Layout for Calculational Technique .

Inner Core	Outer Core	Radial Blanket	Radial Reflector
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Fig. 4.2 - Radial Slice

Axial Reflector
Axial Blanket
Inner Core

Fig. 4.3 - Axial Slice

Fig. 4.4 - Compacted Core Configuration

of the plutonium and uranium density, removal of sodium, and an increase in steel density by 50% was assumed. The raided zone was simulated by a low density steel region (50% density) to simulate empty cans, and secondly, by a complete void. The distorted zone involved 169 matrix tubes (13 x 13), and is thus the largest one considered in the series. The following results were obtained for $\Delta k/k$:

	$\Delta k/k$
Compacted zone assuming complete void above	1.4%
Compacted zone assuming low density steel above it	1.7%

4.2.2 Monte Carlo Perturbation Method (H. Takahashi)

In order to analyze a hypothetical core-disruptive accident, several critical experiments have been proposed. The geometry of a core configuration is much more complicated than critical experiments designed to study them, and neutronic calculations such as Sn calculation and diffusion equation with anisotropy of diffusion coefficient have been used to take into account heterogeneous geometries and voids. Although the Monte Carlo method requires long calculation times, this method is very powerful for analyzing the complex geometry. This will be a very powerful tool for an analysis of HCDA critical experiments and serve as the standard for the evaluation of the other approximate calculational techniques such as Sn and the diffusion methods.

In the analysis of HCDA critical assemblies, the sodium void and the Doppler coefficients as well as the criticality are important. In order to calculate these kinds of coefficients, a perturbation method is a very useful technique.

The code named GEMCM was developed by Takahashi (1970) which calculates geometrical perturbations using a Monte Carlo method. This code has been successful in analyzing the reactivity changes due to a moving reflector of the fast critical assembly for the fast pulsed reactor (Phelps 1972). The GEMCM code was developed from the OSR code, as was the MORSE code system which has the capability of calculating an adjoint equation.

The perturbation calculation is expressed as an integration of the multiplication of flux and adjoint flux over the perturbed regions. Thus, by providing a capability for this integration in the Monte Carlo code, such as the MORSE code, we will be able to use extensively Monte Carlo methods for very complicated geometries with rather short computation time.

As one can find out from the Monte Carlo calculation, multiplication of regular and adjoint fluxes have almost zero measure; therefore, the integration in the sense of Riemann cannot be carried out for this product. In order to carry out an integration in the sense of Riemann, we have to convert the distribution function obtained from the Monte Carlo calculation to the ordinary function by taking some kind of average for the Monte Carlo results.

The simplest average is a histogram which is obtained from the total number of particles that pass through small regions subdividing a whole integrating region. A second averaging method is the use of Fourier transform. A third method is an expansion method using a polynomial function or the orthogonal function for the regular and adjoint fluxes. The integration of the multiplication of both fluxes is easily carried out. A fourth is the method which spreads a δ function type representation obtained by Monte Carlo method into a gaussian type distribution function. By spreading both regular and adjoint fluxes, the multiplication of

both functions has nonzero measure, so that the integration, in the sense of Riemann, can be carried out.

The second method is limited to a simple geometry, and the fourth method requires large core memories so that the first and third methods have been studied. The program code which calculates the histogram type distribution was developed for cartesian, cylindrical and sphere geometries, and the subroutines that calculate the orthogonal polynomial function in each subregion are developed.

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5. FRS Reliability Assessment (Ioannis A. Papazoglou)

This is a summary of the second quarterly progress report of the Fast Reactor Safety Reliability Assessment research program. This research program deals with the uncertainty analysis of the reliability of systems modeled by Markov-chains. A more detailed description of the objectives of this program is presented in the previous quarterly report (Papazoglou, 1977a).

This report presents the mathematical statement of the main problem of this research program, discusses the unfeasibility of an analytical solution and the need for approximate methods, and presents the approximate method of moment matching as well as two approximate methods for the calculation of the moments of a function of random variables.

5.1 Statement of the Problem

The problem of reliability analysis (under uncertainty or not) is to calculate the reliability* of a complex system from existing information about its components. This information is usually expressed in terms of the failure rates and repair rates of the various components. Let the total number of these failure rates and repair rates be m and let x_i denote the i th rate. Then, the reliability of the system at time n is a function of the m variables x_i and of n , or symbolically

$$R(n) = R(x_1, x_2, \dots, x_m, n) \quad (5.1)$$

In the presence of uncertainties, and when the uncertainties are expressed by considering the x_i s as random variables, the mathematical statement of the problem becomes:

"Calculate the probability density function (pdf) of a function R of m random variables with given pdfs."

Even though all the available information about R is included in its pdf, the later is almost never used explicitly as an evaluator of the uncertainties about the reliability of a system. Usually other, more compact evaluators are used. These evaluators are:

- (1) The expected value of the reliability defined by

$$E[R] \equiv \int_0^1 R \cdot f(R) \cdot dR \quad (5.2)$$

where $f(R)$ denotes the pdf of R .

- (2) The variance of R defined by

$$\text{var}[R] \equiv \int_0^1 \{R - E[R]\}^2 \cdot dR \quad (5.3)$$

*Throughout this report we will use the term reliability for collective reference to the reliability, failure probability, availability, unavailability or any other reliability index of a system. Whatever will be said about the reliability holds for any other reliability index.

(3) The probability interval, which gives the probability β , that the reliability of a system will be less than a given value Q , and which is defined by

$$\beta \equiv \Pr \{R \leq Q\} = \int_0^Q f(R) \cdot dR \quad (5.4)$$

(4) The expected utility of the reliability R . This index is more general than the previous ones and it is equal to the expected value of a function of R , $u(R)$, defined in such a way that it reflects the relative importance of each particular value of R . The expected utility is defined by

$$E[u(R)] \equiv \int_0^1 u(R) \cdot f(R) dR \quad (5.5)$$

It must be noted that the first three evaluators are special cases of the fourth. Indeed, the first implies a linear, the second a quadratic, and the third a step utility function for R . Furthermore, from Eqs. (5.2) through (5.5), it follows that if the pdf of R is available, the above-mentioned evaluators can be easily calculated. As it will be seen in the next section, however, the analytical evaluation of the pdf of R is not feasible due to the complexity of the function R and, therefore, approximate methods must be employed. Two such methods that will be described in this report provide directly the evaluators mentioned above.

5.2 Unfeasibility of an Analytical Solution

Whenever a Markov-chain is used to model the probabilistic behavior of a system, its reliability at time n is given by

$$R(n) = \sum_{i \in X} \pi_i(n) \quad (5.6)$$

where $\pi_i(n)$ are the elements of the state-probability vector $\underline{\pi}(n)$ and the summation in Eq. (5.6) extends over all the elements that belong to the set X of operating states of the system (see Papazoglou, 1977b). Therefore, the vector $\underline{\pi}(n)$ is the solution of

$$\underline{\pi}(n+1) = \underline{\pi}(n) \cdot \underline{P} \quad (5.7)$$

and it is given by

$$\underline{\pi}(n) = \underline{\pi}(0) \cdot \underline{P}^n \quad (5.7a)$$

where $\underline{P}$ is the $z \times z$ transition probability matrix of the process. Thus, Eq. (5.6) becomes

$$R(n) = \sum_{i \in X} \sum_{j=1}^z \pi_j(0) \cdot p_{ji}^{(n)} \quad (5.8)$$

where $p_{ij}^{(n)}$ denotes the ij -element of the matrix $\underline{P}^n$, and $\pi_j(0)$ the j th element of $\underline{\pi}(0)$. Since the initial conditions are assumed known with probability one, and since the pdf of a random variable which is a linear combination of other random variables can be calculated rather easily (see, for example, Green and

Bourne, 1972 or Hahn and Shapiro, 1967), the problem defined in section 5.1 reduces into the calculation of the pdfs of the $p_{ij}^{(n)}$ s. This later problem can be stated more generally as:

"Given the pdf of a random matrix, calculate the pdf of a function of this matrix."

Let $\underline{G}$ denote a matrix function of a matrix $\underline{P}$ or

$$\underline{G} = y(\underline{P}) \quad (5.9)$$

where $\underline{P}$ is a random matrix with pdf $f(\underline{P})$. The pdf $h(\underline{G})$ is then given* by

$$h(\underline{G}) = f[y^{-1}(\underline{G})] \cdot |J| \quad (5.10)$$

where J is the Jacobian ($z^2 \times z^2$) of the transformation y [see Eq. (5.9)], and y^{-1} denotes the inverse of this transformation. In other words, in the right-hand side of Eq. (5.10), the p_{ij} s in $f(\underline{P})$ are to be expressed in terms of the g_{ij} s through the inverse transformations

$$p_{ij} = y_{ij}^{-1}(g_{11}, g_{12}, \dots, g_{zz}) \quad i = 1, 2, \dots, z \text{ and } j = 1, 2, \dots, z \quad (5.11)$$

The function $y(\underline{P})$ of interest here, is the n th power of $\underline{P}$ [see Eqs. (5.7a) and (5.8)]. For this function, Eq. (5.10) cannot be used because it implies the availability of a closed expression for the ij th element of the n th root of a square matrix or, equivalently, the availability of closed, inversable expressions of the elements of the n th power of a square matrix in terms of the elements of this matrix. To our knowledge, this problem can be solved only in the following simple cases:

- (a) Small dimensionality of $\underline{P}$ (e.g., 2×2 case).
- (b) Very simple structure of $\underline{P}$ (e.g., diagonal, tridiagonal, etc.).

The matrices encountered in the Markovian reliability analysis are, however, both of greater dimension than 2 and more complex than diagonal or tridiagonal. The analytical method described in this section is not, therefore, feasible and other, approximate methods should be employed.

5.3 Moment Matching

If the first few moments of a random variable are somehow available, its pdf can be approximated by fitting an appropriate distribution to the existing information (expressed collectively in the form of the first few moments). Usually the first four moments are adequate for such a fit. The third and fourth moments define the "shape" or the "form" of the distribution while the first two moments define its parameters. There are several distributions that

* Consider $\underline{P}$ as a z^2 -dimensional random variable and see, for example, Wilks (1962).

can be fitted into a given set of data, ranging from "theoretical" forms such as normal, gamma, beta, log-normal, log-gamma* and student-t, to empirical distributions such as the Johnson family (see Johnson, 1949) or the Pearson family (see Pearson and Hartley, 1954).

The method of moment matching has already been used for the determination of the pdf of random variables in other reliability analyses of nuclear reactor systems. In the Reactor Safety study, for example, the first two moments of the top event of a fault-tree were evaluated by a Monte Carlo simulation. The pdf of the event (whenever needed) was then approximated by a log-normal distribution. Apostolakis and Lee (1976) proposed the calculation of the moments of the top event of a fault-tree through the use of a Taylor series expansion. A distribution from the Johnson family was then chosen to match the first four moments. In this work, both methods (Monte Carlo and Taylor series expansion) were modified and applied in the case of Markovian reliability analysis. Once the moments are obtained, the pdf of the reliability R is approximated by a log-gamma, a beta or a member of the Johnson S_B family distribution.

5.4 Monte Carlo Simulation

Since all four evaluators that are usually of interest (see section 5.1) are expected values of a function of the reliability R , the Monte Carlo simulation method is especially suitable for their direct calculation. This method consists of the following steps:

- (a) Randomly select a set of failure rates and repair rates from their given pdfs.
- (b) With this set of point values, construct the transition probability matrix $\underline{P}$.
- (c) Calculate the n th power of $\underline{P}$, $\underline{P}^n$.
- (d) Calculate the reliability $R(n)$ from Eq. (5.8).
- (e) Repeat steps (a)-(d) N times.

This straightforward Monte Carlo simulation gives good results for the first four moments of R for $N \approx 1200$. For the calculation of the probability intervals [see Eq. (5.4)] and the expected utility [see Eq. (5.5)] where the contribution of the tail of the distribution is important, the straightforward Monte Carlo is not adequate. In those cases a variance reducing technique (importance sampling, for example) is used, or the straightforward Monte Carlo simulation is used to calculate the first four moments and then an appropriate distribution is fitted as was described in section 5.3.

Two drawbacks of the Monte Carlo simulation are the following: (1) the computer time requirements are severe. Methods for reducing the computer time required for each individual calculation were reported in the previous

*A random variable is distributed according to a log-gamma pdf if its negative logarithm is distributed according to a gamma pdf.

quarterly progress report (Papazoglou, 1977a); (2) Sensitivity analysis is not possible since this method provides no information about which of the input variables contributes the most to the uncertainties about the reliability R . Furthermore, if one of the input variables (i.e., its distribution or a parameter of the distribution) changes, the whole simulation must be repeated.

5.5 Taylor Series Approximation

The central idea of this method is to expand the function R [see Eq. (5.1)] into a Taylor series around the expected values $\bar{x}_i$ of the x_i s and retain only the first few terms, i.e.,

$$\begin{aligned} R(n) = & R(\bar{x}_1, \bar{x}_2, \dots, \bar{x}_m, n) + \sum_i (x_i - \bar{x}_i) \frac{\partial R}{\partial x_i} + \frac{1}{2!} \sum_i \sum_j (x_i - \bar{x}_i)(x_j - \bar{x}_j) \frac{\partial^2 R}{\partial x_i \partial x_j} \\ & + \frac{1}{3!} \sum_i \sum_j \sum_k (x_i - \bar{x}_i)(x_j - \bar{x}_j)(x_k - \bar{x}_k) \frac{\partial^3 R}{\partial x_i \partial x_j \partial x_k} + \dots \end{aligned} \quad (5.12)$$

where the partial derivatives $\frac{\partial R}{\partial x_i}$, $\frac{\partial^2 R}{\partial x_i \partial x_j}$, etc. are all evaluated at the point $x_i = \bar{x}_i$, $i = 1, 2, \dots, m$ and the sums are over all indices from 1 to m . If only the terms up to second order are retained and the x_i s are uncorrelated, Eq. (5.12) yields

$$E[R] = R(\bar{x}_1, \bar{x}_2, \dots, \bar{x}_m, n) + \frac{1}{2!} \sum_i \frac{\partial^2 R}{\partial x_i^2} \cdot \text{var}(x_i) \quad (5.13)$$

then, the variance of R is given by

$$\text{var}[R] = E\{R - E[R]\}^2 = \sum_i \left(\frac{\partial R}{\partial x_i} \right)^2 \text{var}(x_i) \quad (5.14)$$

Similar expressions can be obtained for the third and fourth central moments of R .

The main problem in this method is the calculation of the derivatives $\frac{\partial R}{\partial x_i}$, $\frac{\partial^2 R}{\partial x_i^2}$, etc. From Eq. (5.8), it follows that

$$\frac{\partial^m R}{\partial x_i^m} = \sum_{i \in X} \sum_{j=1}^z \pi_j^{(0)} \frac{\partial^m p_{ji}^{(m)}}{\partial x_i^m} \quad (5.15)$$

The partial derivative $\frac{\partial^m p_{ji}^{(n)}}{\partial x_i^m}$ can be calculated recurrently in terms of the $\frac{\partial^k p_{ij}^{(r)}}{\partial x_i^k}$, $k = m-1, \dots, 0$ and $r = n-1, \dots, 1$. A computer code has been developed that calculates the partial derivatives $\left(\frac{\partial^m p_{ij}^{(n)}}{\partial x_i^m} \right)$ of the elements of the n th

power of a matrix with respect to the elements of this matrix. The expected value of R and the next three central moments are then calculated from expressions similar to Eqs. (5.13) and (5.14) including terms up to fourth order.

For the cases considered so far this method yields results that are in good agreement with Monte Carlo calculation. Even though further testing is necessary, it is expected that this method will provide sufficiently accurate results for the following reason. The truncated Taylor series expansion in Eq. (5.12) is a good approximation of R for values $(x_1, x_2, \dots, x_m)$ "near" the mean value $(\bar{x}_1, \bar{x}_2, \dots, \bar{x}_m)$ and a bad approximation for values that differ significantly from the mean. When the expectation of Eq. (5.12) is taken, however, the contribution of the "bad" approximations is expected to be small, since the probability that the point $(x_1, x_2, \dots, x_m)$ will take values far from the mean $(\bar{x}_1, \bar{x}_2, \dots, \bar{x}_m)$ or equivalently, the probability that the various "coordinates" x_i will simultaneously take values far from their respective mean $\bar{x}_i$ is usually small.

Whenever the accuracy of this method is acceptable, it provides a means for a partial sensitivity analysis. Indeed, the coefficient of the variance of x_i in Eqs. (5.13) and (5.14), for example, provides a measure of the dependence of $E[R]$ or $\text{var}[R]$ on the variance of the i th input variable. Furthermore, if a change in the variance (or a higher moment) of x_i is made, the new values of $E[R]$ and $\text{var}[R]$ can be readily obtained from Eqs. (5.13) and (5.14). It must be noted, however, that the mean values $\bar{x}_i$ must remain unchanged. If a change in the values of $\bar{x}_i$ s occurs, then $R(\bar{x}_1, \bar{x}_2, \dots, \bar{x}_m, n)$ and the partial derivatives must be recalculated for these new values of $\bar{x}_i$. These calculations are not lengthy, but the accuracy of Eqs. (5.13) and (5.14) at this new level of $\bar{x}_i$ s must be tested again (probably by a new Monte Carlo simulation).

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III. LIGHT WATER REACTOR SAFETY

SUMMARY

The post-dryout, non-equilibrium vapor generation source correlation has been checked successfully against water data at near critical pressure representing an extrapolation by a factor of three over the development range. A generalized form for the vapor source has been analytically derived for the case of one phase in uniform non-equilibrium and interfacial transfers expressed through a heat transfer coefficient given by, (symbols defined in section 1.1)

$$\Gamma_v = \rho_m \frac{x_e - x}{\tau}$$

where the time constant τ is given in terms of the generalized relaxation number, $Nr_k = 6h(1 - \alpha_k)/\delta C_{pk} x_k \Gamma_e$, as $\tau = \rho_m / \Gamma_e Nr_k$. Phase-k is the phase in non-equilibrium and Γ_v represents both evaporation ($x_e > x$) and condensation ($x_e < x$). Nr_k has previously been correlated for the case of post-dryout heat transfer. A survey of flashing non-equilibrium vapor generation has been completed and will be issued shortly. Initial modeling efforts have begun.

The overall test facility flow circuit layout has been completed and the thermodynamic evaluation of the loop modules accomplished. Overall mechanical parts list have been determined and considerable parts acquired. Placement of the facility modules has been completed and appropriate modifications are in progress. Modifications of the building have either been initiated or are in progress and installation of the cooling tower has been initiated.

Considerable progress has been made in development of the test instrumentation in both mechanical and electronic development areas. Initial methods of joining and shaping small optical fibers have been developed. Evaluation of electronic circuitry has begun including the completion of a two-phase, bubbly test rig for evaluation of the hydrodynamic interactions. The signal analysis system has been received and considerable hardware and software installation has been accomplished. The operating system and all non-data I/O devices are operational. The slow speed data system has been partially installed and the high speed system ordered.

In the THOR program several demonstration test cases have been analyzed to demonstrate the feasibility of lumped-parameter modeling of reactor components. Component coupling with lumped-parameter formulation has been achieved. Good agreement has been obtained between finite-difference solutions, solutions by the method of characteristics and lumped-parameter solutions. Savings of computing time have been found (tentatively) to vary between 6 (some conditions of forced flow) and 300 (free convection), in the calculation of lumped-parameter models, when compared with the finite difference method.

Progress on component modeling for the reactor core, the downcomer and for adiabatic pipes are presented. The finite difference algorithm for thermohydraulics in pipes has been extended to accommodate closed-loop networks. The system balance equations for the single loop PWR system have been prepared for programming.

Work has been completed on the service routines for heat transfer and friction factor.

Brookhaven National Laboratory has been assigned the task of following water reactor safety related analysis and computer code development in foreign countries and appraising that development. A list of forty-two codes has been compiled and the Australian code NAIAD has been implemented on the BNL 7600. Reactor Safety Research problem 2 has been completed.

1. Light Water Reactor Thermal/Hydraulic Development Program (O. C. Jones)

1.1 Development of Constitutive Relations

1.1.1 Analytical Modeling

(a) Effects of Heat Flux (O. C. Jones and A. L. Swoboda)

Progress has continued on a low level of effort in comparison of the correlation given by Jones (1977) with existing data. The correlation has been checked against the high pressure ($p_r = 0.94$) data of Swenson (1967), representing a factor of three extrapolation over the reduced pressure range used in its development. Typical results are shown for the wall temperature prediction in Fig. 1.1.

(b) General Considerations (O. C. Jones)

There has been considerable discussion of the actual form the actual vapor source term Γ_v should take. For instance, according to Kroeger (1976), Zuber suggested the form

$$\Gamma_v = \Gamma_e \left(1 + \frac{C_e - C}{\tau^*} \right) \quad (1.1)$$

for flashing. Wegener (1969) has used

$$\Gamma_v = \rho_m \frac{(C_e - C)}{\tau^*} \quad (1.2)$$

while Hirt and Oliphant (1976) specify

$$\Gamma_v = K^* S u_v^{1/2} (\rho_g - \rho_v) \quad (1.3)$$

The terms defined by these sources include the volumetric vapor source, Γ_v , the equilibrium source, Γ_e , the equilibrium and actual static mass concentrations, C_e and C , the relaxation time constants in dimensional terms, τ , and dimensionless terms, τ^* , the interfacial area density, S , the specific internal energy of the vapor, u_v , and the actual and saturation vapor densities, ρ_v and ρ_g .

K^* is an empirical dimensionless constant according to Hirt and Oliphant (1976). On the other hand, Malnes (1975) has semi-empirically obtained

$$\Gamma_v = f(p, T, \alpha) \Delta T^2 \quad (1.4)$$

where p , T , and α are pressure, temperature, and void fraction, and ΔT is the non-equilibrium temperature difference.

Equation (1.1) was proposed considering delayed nucleation in flashing and the need for Γ_v to be larger than Γ_e at some point in order that equilibrium be approached. Equation (1.2) is basically a first order static chemical reaction while (1.3) has no physical basis and is, in fact, dimensionally inconsistent. Equation (1.4) was derived semi-empirically through considerations of the thermal boundary layer control of bubble growth in uniformly superheated liquids, but does not, therefore, apply to flashing in variable pressure fields.

For the case where one phase is at saturation and the other is not, the non-equilibrium may be expressed as

$$(x_e - x) = \frac{\bar{C}_{pk} x_k \Delta T_{ks}}{\Delta i_{fg}} \quad (1.5)$$

where x is the vapor quality and x_k is the mass flow fraction of phase- k . The non-equilibrium superheat or subcooling is given by $\bar{C}_{pk} \Delta T_{ks}$ whereas Δi_{fg} is the latent heat. Equation (1.5) applies only where phase change occurs due to bulk difference between saturation and phase- k , such as may exist in post-dryout, bulk flashing, or bulk condensation, but not subcooled boiling. In the cases mentioned, evaporation (or condensation) is likely to occur in some part from spheroidal surfaces of diameter δ . If the interphase exchange can be described in terms of a heat transfer coefficient, the vapor source may be expressed as

$$\Gamma_v = \frac{6h}{\delta \bar{C}_{pk}} \left(\frac{1 - \alpha_k}{x_k} \right) (x_e - x) \quad (1.6)$$

such that non-equilibrium relaxation number for phase- k not at saturation is given by

$$Nr_k = \frac{6h(1 - \alpha_k)}{\delta \bar{C}_{pk} x_k \Gamma_e} = \frac{\Gamma_v}{\Gamma_e (x_e - x)} \quad (1.7)$$

and the generalized expression for relaxation of the non-equilibrium is

$$\frac{d(x_e - x)}{dx_e} + Nr_k (x_e - x) = 1 \quad (1.8)$$

The derivation of (1.8) is directly analogous to that given by Jones (1977). For evaporation, $x_e > x$ and $\Gamma_v > 0$ whereas if condensation occurs $x_e < x$ and $\Gamma_v < 0$.

The generalized inhomogeneous relaxation equation may be written as

$$\frac{d\eta}{dt} + \frac{\eta}{\tau} = H \quad (1.9)$$

which for a steady flow process becomes

$$\frac{d\eta}{dz} + \frac{\eta}{v_m \tau} = \frac{H}{v_m} \quad (1.10)$$

where v_m is the characteristic velocity. Since $d/dz = (d/dx_e) (dx_e/dz)$ (1.10) becomes

$$\frac{d\eta}{dx_e} + \frac{G}{\Gamma_e v_m \tau} \eta = \frac{HG}{\Gamma_e v_m} \quad (1.11)$$

Comparison with (1.8) shows that

$$H = \frac{\Gamma_e}{\rho_m} \quad \text{and} \quad Nr_k = \frac{\rho_m}{\Gamma_e \tau} \quad (1.12)$$

Now since (1.8) may also be written as

$$\frac{d(x_e - x)}{dx_e} + \frac{\Gamma_v}{\Gamma_e} = 1 \quad (1.13)$$

we immediately have

$$\Gamma_v = \Gamma_e Nr_k (x_e - x) \quad (1.14)$$

which is a more generalized form of that given previously (Jones 1977).

Finally, comparison of (14) with (12) yields

$$\Gamma_v = \rho_m \frac{(x_e - x)}{\tau} \quad (1.15)$$

where

$$\tau = \frac{\rho_m \bar{C} \delta x_k}{6h(1 - \alpha_k)} \quad (1.16)$$

as the generalized expression for the vapor source term in quasi-steady flowing systems. Equation (1.15) shows that the expression of Wegener (1969) is most nearly correct except that the flowing quality rather than the static value should be used. The two (quality and concentration) are related through the slip ratio as

$$\frac{x}{1-x} = \frac{c}{1-c} \frac{v_v}{v_l} \quad (1.17)$$

so that (1.2) is identical with (1.15) for the case of no slip, that originally proposed by Wegener (1969).

It should be mentioned that the actual source term may be either larger or smaller than the equilibrium value. For instance, in the nucleation delay length in a flashing system obviously $\Gamma_e > 0$ whereas $\Gamma_v = 0$. At the start of nucleation there is generally insufficient interfacial area for significant vaporization to take place and $0 < \Gamma_v < \Gamma_e$. Further downstream however, $x \rightarrow x_e$, and the only way this can happen is for Γ_v to be greater than Γ_e . Exactly opposite considerations exist for the case of subcooled boiling where for upstream $\Gamma_v > \Gamma_e$ while as $x \rightarrow x_e$, $\Gamma_v < \Gamma_e$.

(c) Effects of Pressure Change (P. Saha)

A literature survey of the two-phase, steam-water critical flow models have been completed in this quarter (Saha 1977). The review clearly shows that the effect of thermal non-equilibrium is important and must be considered for calculating the discharge of initially subcooled or saturated water through short pipes. There is, however, uncertainty whether the pipe length, L , or the pipe length-to-diameter, L/D , should be governing parameter in determining the degree of thermal non-equilibrium. Nevertheless, there are three types of models to take into account the effect of thermal non-equilibrium during a flashing flow. They are the following:

1. Henry (1970a, 1970b, 1971) have made a direct assumption regarding the actual vapor qualities and various derivatives at the choking plane without describing the mechanism of vapor formation. Although these models have been found successful in predicting steady-state critical flow data, their use in transient situations may not be applicable mainly because of their non-mechanistic nature.

2. Edwards (1968), Malnes (1974), Wolfert (1976) among others have attempted to model the problem of non-equilibrium flashing flow from a mechanistic view point. Some of the models include a bubble nucleation delay, a rate of bubble nucleation, a bubble growth law inapplicable in variable pressure fields (Jones 1976), a bubble density, and even an effect of dissolved gas. Unfortunately, none of the models present any general correlation for any of these parameters, and further work is needed in this area.

3. Finally, Boure (1973) have proposed that the general constitutive equation for the rate of vapor generation must include terms containing derivatives of dependent variables like pressure, enthalpy, etc. The reason is that only these terms of the constitutive equation enters the system determinant which when vanishing at the choking plane yield all real characteristics. However, no widely accepted functional form of such a constitutive equation is available yet.

It is clear from the review that an improved modeling of the non-equilibrium effects during the flashing flow is badly needed. Work in this direction has begun at BNL.

1.2 Design, Construction and Checkout of Experimental Equipment

(a) Flow Circuit (S. A. Fogelson, J. H. Klein, A. Losi)

The detailed engineering and drafting work initiated during the previous quarter (Jones 1977) has continued. Major efforts, discussed in what follows, were directed toward defining and verifying the allowable operating conditions of the components of the modules obtained during the previous quarter, and developing a flow diagram and equipment arrangement for the pressurized closed water loop. Manufacturers were contacted to determine availability, costs and delivery times for piping, fittings, control valves, and water heaters not included in the modules. Sizes for the control valves and water heaters were determined during the period, and the modules were partially stripped of superfluous components and placed according to the equipment arrangement.

The allowable operating capabilities of almost all the components were available from specification sheets obtained with the modules or by brief inquiries to their manufacturers. However, determining the operating temperature and pressure capabilities of the circulating water pumps and accumulator tanks (one of which is to be used as a flash tank) was more involved.

Two sizes of pumps were obtained with the modules: one 75 HP, 300 ft. TDH (~130 psi), 600 gpm; the other 30 HP, 185 ft. TDH (~80 psi), 550 gpm. After referring to records, the manufacturer provided the following information. The 75 HP pumps are an old model whose seals can be changed to allow operation only up to 300° F, maximum, and whose case pressure cannot

exceed 140 psig. This latter restriction limits the maximum pump suction pressure to about 10 psig and suction temperature to less than about 230°F for the test loop flows anticipated. Using these pumps in the loop would therefore limit the maximum test section temperature to about 240°F. On the other hand, the 20 kw pumps are also adaptable for operation at about 300°F with a seal change, and can probably be operated at higher temperatures if seal cooling is provided. In addition, these pumps are newer models which have a maximum allowable case pressure of 175 psig. Using these pumps, with seal cooling, would permit a maximum suction pressure of about 6.6 bar-q and suction temperature of about 330°F, and would thus permit a maximum test section temperature of over 300°F to be obtained. The loop has been designed to employ these smaller head pumps. Additional care was taken in the design of the main loop piping to insure an adequate pressure drop across the control valves to prevent flow instabilities from occurring in the main stream during the onset of flashig in the test section.

No information was available concerning the two accumulator tanks either in the module specifications or stamped on the vessels. To determine if they are usable at desired design conditions of 150 psia, 350°F, preliminary wall stress calculations were performed based on wall thickness measured ultrasonically. The calculations indicated that the tanks were acceptable at design conditions. To further confirm this, a procedure has been defined, in accordance with the ASME Boiler and Pressure Vessel Code, to hydraulically test one of the tanks. Basically the test consists of increasing the internal tank pressure in small increments up to a test pressure 490 psig while noting any residual tank distortion. This test pressure includes a derating factor of 1.3, to account for the test being carried out at room temperature, whereas the actual operating temperature will be as high as 340°-350°F, and safety factor of 2.5. To date, this tank to be tested has been removed from its module and fixtures, and fixtures are being made to close-off or replace those which will not be used in the final configuration.

Figure 1.2 shows the detailed schematic flow diagram developed for the pressurized closed water loop employing the available module components. Expected operation of the loop may be described with reference to Fig. 1.2 as follows. Circulating, subcooled water enters four electric heaters having a 520 kW capacity where it is heated almost to saturation. Current design consists of four horizontal, stainless steel, pipe bodies in series. Two will have two 100-kW electric heaters, one inserted in each end, one will have one 100-kW heater inserted in one end, and one will have one 20-kW heater inserted in one end. The two 200 kW units are to be operated on/off, while the 100 kW unit is to provide coarse temperature adjustment and the 20 kW unit, fine adjustment. Particular attention is being paid to close control of the temperature due to the dependency of the flashing phenomena on this parameter.

After flowing through the heaters, the liquid enters the converging-diverging test section where its pressure drops below that corresponding to saturation due to friction and acceleration and it begins to flash. The characteristics of the vapor generation are monitored as discussed by Jones (1977).

The mixture of liquid and vapor then enters the flash tank where a cold water spray condenses the vapor and permits a constant preselected temperature and pressure to be maintained. The cold water spray is provided by flow through a bypass line from the discharge of the circulating water pumps. In the bypass line, primary bypass coolers decrease the temperature of the bypass flow to about 80°F-90°F before it is sprayed into the flash tank. Waste heat is discharged to a secondary loop having a wet cooling tower.

To maintain mass balance in the flash tank, liquid is withdrawn at the bottom. Since the liquid is saturated, a small amount of cold water at about 80°F-90°F is injected into the exit pipe to prevent flashing in the downstream line. The injected water is provided from a secondary bypass line having a cooler which rejects heat to the secondary loop and cooling tower previously noted. This bypass line also has a water purification unit which provides a continuous means of maintaining desired water purity.

A second injection point just upstream of the pump provides the additional cooling water necessary to insure that the mainstream liquid is sufficiently subcooled to prevent pump cavitation. To minimize power requirements, a maximum of ~10°F overall main stream temperature decrement produced by the two injected flows will be allowed.

For low main stream flows, an additional pump cooler is included to remove the pump heat. Waste heat from this cooler is also rejected to the secondary loop and cooling tower previously noted. At low flow, most of the pump motor energy goes into heating the main stream liquid. To prevent this heating from becoming excessive and causing cavitation, part of the main stream flow is recirculated from the pump discharge to the pump section through the pump cooler. A temperature rise across the pumps of less than about 10°F is thus insured.

After discharging from the circulating water pumps, the main stream liquid flows through the main control valves and flow meters, past the junction with the pressurizer and into the electric heaters to complete the loop. The pressurizer serves three functions: (1) it permits maintaining a selected pressure at the entrance to the heaters, and thus at the entrance to the test section, (2) its liquid level provides a control point for maintaining constant flow in the loop, and (3) it provides a means of adding liquid to the loop or removing liquid from it, either for makeup or for a change in operating conditions in the flash tank.

For adding liquid to the loop during operation, a small positive displacement pump is provided. The liquid from the pump discharge first flows through a preheater coil wrapped around the pressurizer body. This permits the make-up liquid to approach the temperature of the liquid already in the pressurizer and minimizes the effects of any thermal shock and long-term pressure transients on the system.

Makeup water is provided from the existing laboratory supply. It is filtered and purified, and either piped directly to the makeup pump or to the

storage tanks and later to the makeup pump. The storage tanks also provide a place to store the high purity water from the loop if it becomes necessary to completely shut the system down for modifications or repairs.

Small vacuum pumps connected to the top of the pressurizer and flash tank provide coarse evacuation of the entire system to expedite initial filling and eliminate non-condensable hideout.

(b) Test Section (W. J. Leonhardt, J. H. Klein, J. J. Seeberger)

Design and development work continues on the test section. A test rig was constructed to test the sliding seals as described in the last quarterly progress report (Jones 1977). Testing was conducted and, based on the results of this test, the sliding seal design was judged not practical for this application. Attention has thus shifted to redesign involving a different but more complicated and expensive design.

Development work began on locating all the mechanisms for traversing the probe rake within pressurized chambers whose pressure will be about equal with that of the test section. This is expected to provide a smooth, leak-free operation since conventional seals (i.e., o-rings) can now be employed. Development is presently continuing in this area.

An inflatable seal was designed and silicone rubber was chosen as a trial material to operate in our testing environment. This seal closes around the probe rake and separates the test section from the pressurized chambers mentioned above. An order has been placed for a sufficient amount of seal, and delivery is expected early in the next quarter. A test rig was designed and constructed to verify the suitability of the present seal design and testing now awaits the seal delivery.

As was mentioned in the last quarterly report, hydrodynamic flutter of the probe rake requires consideration. A test rig was designed, constructed, and is presently being modified to include instrumentation to measure vibration effects. A small test loop was constructed to accommodate this test rig and a probe rake mock-up remains to be constructed.

(c) Global Densitometer (N. Abuaf, W. J. Leonhardt)

The source for the detection system has been selected as Thulium (Tm^{170}) with a half life of 127 days and γ -energy of 84 Kev. This source is both obtainable and renewable on site (BNL). Two channels of densitometry will be used and may be expanded to ten channels in the future. Physical source dimensions are 1 mm diameter by 1 mm long. Source strength will be from 1 to 10 curies (considering the required dynamic response accuracy) and will be determined by in-house testing as described below.

The selection of detectors has been finalized using Cadmium-Teluride solid state γ -detectors. These will be used in the pulse mode. Their physical size is 4 mm square by about 2 mm thick.

The geometry of the collimation and source detector set up is illustrated in Fig. 1.3. This allows a γ -beam of approximately 0.1 inches (on the average)

to pass through the test section, which satisfies the proposed global γ -densitometer system requirements.

A bench test set up has been developed in order to determine the effects of the source strength, the collimation and set up geometry, γ -ray scattering by cylindrical geometry and tube temperature on the detector output. The test will be conducted with stainless steel tubes, 2" and 1-1/4" diameter, with a 0.020" wall thickness as shown in Fig. 1.4. This will provide a geometry which closely follows the actual test section design. Water will be simulated by plexiglass cylinders inserted inside the stainless steel tubes and different hole patterns will be drilled into the plexiglass to simulate different degrees of void fraction. The outcome of this testing is expected to provide the information necessary to optimize the γ -ray absorption system.

(d) Facilities (J. H. Klein)

A combination of temporary office trailers and permanent building modifications have been completed, and the entire group has now been relocated adjacent to the experimental facilities.

Funds and authorization for converting the old emergency generator room has been obtained. Plant Engineering is in the process of assigning outside contractors to this work. The loop cooling tower has been sized and the procurement and installation has been assigned to Plant Engineering. Funding and authorization for construction of the instrument storage room was obtained, and work has begun in this area.

1.3 Development of Instrumentation Techniques

1.3.1 Analytical Modeling

No effort has been spent in this area during this quarter due to work efforts placed in hardware design.

1.3.2 Design, Construction, and Checkout of Experimental Equipment

(a) Local Instruments (N. Abuaf and A. L. Swoboda)

The development of the optical probe has been continued in order to understand its response to optical (geometrical) and hydrodynamic effects (i.e., interfacial distortion, passage of the bubble, wetting). Considerations related to the electrical response of the system were reported by Jones (1977). Some of the limitations of the existing optical probes were also described by Jones 1977.

The operation principle of the optical probe as based on Snell's law

$$n \sin i = n_o \sin i_o$$

seems quite basic. For a glass rod $n_o = 1.62$, and $i_o = 45$, so the light beam is reflected if $n < 1.15$ or refracted if $n > 1.15$. The probe thus enables one to detect the gas and liquid phases if $n_g < 1.15 < n_l$.

Three optical probe configurations have been used in the literature:

1. The glass rod probe of Miller and Mitchie (1969) terminated as a cone.
2. The fiber bundle probe of Hinata (1972) terminated as a glass rod.
3. The U-shaped fiber probe of Danel and Delhay (1971). A second version was also used by Galaup and Delhay (1976) where the sensing element of the original probe was replaced by a faster response phototransistor (200 kHz).

Two problems can be identified common to all the probes mentioned above:

1. The static level of the signal and fluctuations caused by the passage of the bubbles varied with the void fraction (Jones 1977). The authors used a double triggering system while the threshold levels were determined from a calibration against a global void fraction measurement performed with a γ -ray absorption technique.
2. The penetration time of the probe into the bubble was longer than the time it took for the probe to be completely immersed in the liquid again. These effects varied with bubble size and flow velocity. Hinata (1972) observed an opposite trend in a mercury air system. All these effects are attributed to electronic or hydrodynamic response problems. Now let us present some preliminary considerations on the optical probe performance:

1. Optical geometrical considerations.

Figure 1.5 represents the U-shaped sheathed fiber similar to that used by Danel and Delhay (1971). For the case of the glass fiber immersed in water, it is clearly seen that the rays coming close to the outer boundary are reflected back. In addition to this, due to the curvature of the probe, the rays refracted into the water seem to be focussed somewhere in front of the probe. Depending upon the local void fraction, light from this region will be scattered and may reenter the probe. The net result would seem to be a high but variable d-c level.

2. Wetting and effect of film thickness of the probe.

The next question to be answered is: "what the minimum thickness of the water film that still can exist on the probe tip, causing the probe to see an all water condition (ideally complete refraction of the light)?" Figure 1.6 depicts a simple consideration of this question for the Miller Mitchie probe (1969). A simple calculation using Snell's law and linear variation of the film thickness along the probe tip, shows that a water film thickness of 0.0004" and larger at the edge of the probe tip, will cause the probe to see an all water situation (ideally complete refraction).

Based on these preliminary considerations, the important effects and parameters to be investigated are listed below in order to reach an understanding

of the operational characteristics of the optical probe.

Electronic response of the detecting system.

Distortion of the bubble as it passes the probe.

Variation of liquid film thickness on the probe tip, during the passage of the bubble and the respective response of the optical probe.

In order to conduct a systematic investigation of these effects the following steps have been pursued.

1. For the light sources, the high radiant intensity emitters in the visible and near IR (670 nm) sources using a GaAsP on GaP LED chips are being checked. Their physical sizes are small, the light beam is sufficiently narrow yet broad enough to simplify optical alignment. They can be modulated at high frequencies (above 4 MHz).

Concerning the light detectors, silicon planar PIN photodiodes are being checked. They are ultra fast light detectors (rise and fall time 1-300 n sec) for visible and infrared radiation.

The geometry of the fiber probe is still being investigated to minimize optical (geometrical) effects, hydrodynamic disturbances and, most important, production techniques and costs.

The electronic response bandwidth of the sensing system will be checked by driving the LED through a frequency generator (up to 10 MHz), trying to generate rise and fall times comparable to the bubble sizes and velocities in the two-phase flow. Both the LED modulation and the output of the sensing system will be evaluated and improved in a dual beam scope.

For purposes of testing the initial circuitry, an immediately available, lower speed photodiode (19 μ sec rise time) is being tested in conjunction with the high speed emitter. The effect of the response time is seen in Fig. 1.7 where the upper trace represents the excitation to the LED, and the lower trace indicates the photo response to (a) 1 kHz and (b) 5 kHz. The electronic distortion is caused by the limited bandwidth (< 50 kHz) of the photodiode.

2. To check the hydrodynamic phenomena affecting the probe response, an air bubble generator was built as shown in Fig. 1.8. It consists of a plexiglass tube filled with water, and a hypodermic needle attachment at the bottom. Different size bubbles can be generated. The size of the bubbles and their velocities will be determined by the photographic techniques, and compared to predictions existing in the literature (Govier and Aziz 1972). These calibrated bubbles will be used for studying the response of the optical probe to the various hydrodynamic parameters (i.e., bubble size and velocity, interphase distortion, liquid viscosity, surface tension, etc.).

(b) Signal Analysis System (G. A. Zimmer)

The Hewlett Packard 9640 System, the primary component of the signal analysis system, was accepted following satisfactory completion of the

acceptance tests performed by the vendors customer engineers. The Varian Printer/Plotter was interfaced to the system and a series of test programs were executed to verify the system performance. The Printer/Plotter satisfactorily completed the tests and produces report grade listings and plots. The instrument, however, has been found to be sensitive to the relative humidity of intake air and during the cold dry weather in early February, it was necessary to humidify the intake air to maintain report grade output. The high resolution integrating digital voltmeter was received and interfaced to the computer via the Universal Interface Bus (IEEE Standard 488). A demonstration program was written and executed to test the Bus and the instrument. Both the Bus and the instrument performed satisfactorily, and the instrument was accepted. The Bus is an integral part of the 9640 system. The Crossbar Scanner (obtained from GSA excess) was interfaced to the high resolution voltmeter (but not to the computer) to provide the external trigger necessary to initiate a measurement when used with scanning device. The trigger pulse is initiated 22 milliseconds after the crossbar solenoids are energized to allow for contact bounce and transient signal fluctuations as a result of switching. The instruments, which form the nucleus of the slow speed, steady state data logging capability of the signal analysis system were tested with the voltmeter controlled by the computer and the scanner in local or front panel control since its computer interface has not been received. The tests were satisfactory, confirming the system design prediction that the two instruments could be combined to form a high resolution steady-state data logging subsystem. Tests were also run on the Relay Register, used for on/off control of external devices and the Dual 8-bit Digital to Analog Converter, used to output data to an oscilloscope or vector terminal. Both of these devices were also accepted.

Driver programs for the following devices have been debugged, installed and are considered operational within the system: the Universal Interface Bus, Printer/Plotter, Relay Register and High Resolution Voltmeter. The following engineering routines have been added to the system: Degree, a routine to convert IC or CA thermocouple signals to degrees F; Steam, a group of routines which calculate the thermodynamic properties of water as a function of pressure or temperature or pressure and temperature. Several general purpose routines have been implemented to aid the user in manipulating or acquiring data. These routines are: SHIFT, which performs left and right bit or byte shifts; IADRS, which calculates the absolute address of a word, a necessity when working with the voltmeter and CNVRT, a routine which converts 8-4-2-1 or 4-2-2-1 BCD codes to a real number.

The specification for the multiplexed ADC subsystem, which was discussed in the Second Quarterly Progress Report (Jones 1977), was revised after the quotations were received from the various vendors. The revised specification extended the per-channel bandwidth to 0.5 MHz, to take advantage of current state-of-the-art technology. Delivery of the system is scheduled for the first of June. Quotations have also been solicited for a precision voltage standard, a quartz manometer and electronic ice point thermocouple reference junctions. Delivery of these items is expected to be late May.

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Figure 1.1 Comparison of the data of Swenson et al. (1965) at $p_r = 0.94$ with the non-equilibrium post-dryout correlation.

Figure 1.2 Schematic diagram of test facility layout

Figure 1.3 Schematic diagram of γ -source and detector system

Figure 1.4 Sketch of test section mockup used to test the γ -source and detector system

Figure 1.5 Geometry of U-shaped optical probe showing paths of various light beams when submerged in water

Figure 1.6 Sketch of 45° probe tip with water film

$$\text{Snell's Law } 1.61 \sin(45^\circ) = 1.33 \sin \alpha_1 \quad \alpha_1 = 58.9^\circ$$

$$\alpha_2 = 180 - (\beta - 45) - 31.1 - 90; \quad \alpha_2 = 103.9 - \beta$$

What is the value of β for reflection

$$1.33 \sin(103.9 - \beta) = 1.01 \sin(\pi/2)$$

$$\beta = 54.5^\circ$$

Assume Linear Variation of δ along Probe Tip

$$\delta = 0.002 \times 1.414 \sin(54.5 - 45)$$

$$\delta = 4.6 \times 10^{-4} \text{ in.}$$

Figure 1.7 Response of trial optical probe source-detector circuits to an
 (a) 1.0 kHz square wave source input
 (b) 5.0 kHz square wave source input

Figure 1.8 Schematic of bubble generator

2. THØR CODE DEVELOPMENT

(P.G. Kroeger, G.S. Srikantiah and W. Wulff)

The THØR Code Development program is designed to produce a computer code for the prediction of accident-induced thermohydraulic transients in water-cooled nuclear reactor systems (Wulff, W. et al. 1975-1976, Levine, M.M., June 1976).

Results are presented on the comparisons between lumped-parameter and discrete-parameter formulations for loops and pipe networks. Progress on component and system modeling is presented.

2.1 Demonstration Test Cases for Lumped-Parameter Formulations

One of the objectives of the code is to accommodate individual hydraulic component models by both finite difference and by lumped-parameter representations. Two series of demonstration test cases are being analyzed, for pipe networks and for closed loops with plena in order to establish (i) the feasibility of coupling hydraulic components with lumped-parameter descriptions, (ii) the accuracy of the results from lumped-parameter, relative to that from discrete-parameter formulations, and (iii) the possible potential of saving computing time.

Three test cases are being analyzed on pipe networks, namely two pipes of equal diameter in series, two pipes with unequal diameters in series and three pipes in parallel, connected with T-branches. The solutions obtained from the lumped-parameter formulation has been compared against solutions obtained from the finite-difference formulations (Wulff, Dec. 1975).

The second series of demonstration test cases involves three loop configurations with forced and free convection flows through combinations of a constant pressure plenum, a constant volume plenum, the later with or without a break, a pump and two pipes, one with heating and the other with cooling. The results obtained from the lumped-parameter formulation are compared with results obtained from the numerical solution based on the method of characteristics.

It must be emphasized that the modeling techniques, specifically the simple profile functions and the restrictive simplifying assumptions employed for the demonstration test cases differ in part from those employed in the THØR code development. Therefore, the comparison presented here is not necessarily indicative of results to be expected from the THØR code.

2.1.1 Pipe Networks (G. Srikantiah, U.S. Rohatgi, A. Dubow and S. Lekach)

The lumped pipe test problems to be considered in this study are illustrated in Figure 2.1.1. In all test problems homogeneous, two phase one component equilibrium flow as well as perfect gas flows are considered.

It is shown that the coupling of such components is made possible by the differentiation with respect to time of some of the profile equations.

Figure 2.1.1 Pipe configurations

2.1.1.1 Two Pipes in Series

In this problem, the coupling of two constant area, vertical pipes is considered. The simplifying assumptions are: (i) saturation properties are computed from volume averaged pressure, (ii) local vapor concentration is replaced by its volume averaged value in the mixture density except in the inertia term, (iii) linear spatial variations for vapor concentration and pressure are employed in each pipe. By equating the expressions for the mass flow rate at the junction, a first order ordinary differential equation in time (ODE) for the junction pressure is derived. In this derivation the differentiated profile equations for the pressure in each pipe have been used. The resulting set of ODE's and algebraic equations (AE) have been solved to obtain the transient behavior of the flow. The boundary conditions employed in this problem are prescribed pressures at inlet and outlet and prescribed enthalpy at the inlet to the two pipes in series. The pressure transient imposed is comparable to that occurring in a plant blowdown. For purposes of comparison, distributed parameter computations were performed using 24 nodes and our implicit Euler scheme (Wulff, Dec. 1975).

Results are shown in Figures 2.1.2 through 2.1.6. Figures 2.1.2 and 2.1.3 and 2.1.4 show the distribution of pressure, velocity and vapor concentration along the lengths of the pipes at different times for both lumped and distributed parameter calculations. This set of figures is quite significant in that it shows that the lumped-parameter model profile computations accurately predict the changes in profile shape during the transient. Figures 2.1.5 and 2.1.6 show the computations for a perfect gas model for both lumped and distributed parameter models. The agreement of lumped-parameter computations with the distributed parameter calculation is excellent in all cases. The computing time requirements of the lumped parameter cases were 1/10 of the time required for the distributed parameter calculations of the same transients.

2.1.1.2 Two Pipes with Area Change

In the second test problem a contraction has been introduced between the two pipes. The contraction is presently described by the following equations.

Mass balance: $G_1 A_1 = G_2 A_2$

Momentum balance: $p_1 - p_2 + \frac{G_1^2}{2\rho_1} - \frac{G_2^2}{2\rho_2} - \Delta E = 0$

$$\Delta E = \left\{ \left(\frac{A_2}{A_1} - 1 \right)^2 + \left(1 - \frac{A_2}{A_1} \right) \zeta \right\} \frac{G_2^2}{\rho_2}$$

Energy balance: $h_2 - h_1 + \frac{1}{2} \frac{G_2^2}{\rho_2^2} - \frac{1}{2} \frac{G_1^2}{\rho_1^2} = 0$

Figure 2.1.2 Two pipes in series, pressure profiles

Figure 2.1.3 Two pipes in series, mixture velocity profiles

Figure 2.1.4 Two pipes in series, vapor concentration profiles

Figure 2.1.5 Two pipes in series, perfect gas case

Figure 2.1.6 Two pipes in series, perfect gas case

The loss coefficient ζ has been taken to be 0.5. In this test problem the time-derivative of the intermediate pressure p_2 has been computed from history. The results obtained are shown in Table I.

2.1.1.3 Three Pipes in Parallel

In this section are discussed first the lumped-parameter formulation and then the modifications that were necessary to extend our discrete-parameter computing algorithm for the treatment of closed-loop networks.

(a) Lumped-Parameter Formulations

The last example considered in this series is a network formed of three pipes and four T-branches as shown in Figure 2.1.1. The flow directions considered are as indicated which give rise to two diverging and two converging T-branches connecting three pipes in parallel. The boundary conditions are prescribed pressures at the inlet and outlet and a prescribed enthalpy at the inlet. T-branch formulation follows Ginzburg (1958).

The pressure derivative terms in each of the three pipes are evaluated by applying the pressure-time history. This leads to 6 ordinary differential and 25 algebraic equations which are to be solved at each time step. Comparisons are being planned with the results of a distributed parameter computation.

(b) Discrete-Parameter Modeling of Pipe Networks

The discrete-parameter code has been extensively used to provide comparison results for the lumped-parameter demonstrations tests. A typical sample problem was first solved by both the discrete implicit finite difference method and the discrete method of characteristics in order to check out the implicit method. Then the implicit finite difference method was employed for a series of calculations on two-phase flow through a simple pipe with varying boundary conditions. The results were compared with solutions from the lumped-parameter approach and showed a good agreement.

In order to provide comparison results for another series of demonstrations (three pipes in parallel) the discrete parameter method had to be modified to be able to handle hydraulic networks other than a series of connected pipes with the possibility of one T-branch. The main modification has been to express the solution vector as a linear combination of the boundary conditions. Once the coefficients of this linear combination are obtained the solution vector can be calculated if the boundary conditions are known. The boundary conditions between components also have to obey the coupling equations between components. This procedure has been implemented into the discrete code and has been applied to the three-pipe configuration for comparison with the lumped-parameter solutions obtained for the same configuration.

2.1.2 Loop Problems (P.G. Kroeger, J.H. Jo)

The geometry of the loop problems to be considered are shown schematically in Figure 2.1.7. Configurations 1 and 2 have been investigated to date.

TABLE I

Data Comparison for Two Pipes with Area Change

Prescribed Conditions

$$P_1 = 100.0 \text{ b}$$

$$P_4 = 90.0 + 8 \exp - \tau/12 \text{ b}$$

$$C_1 = 0.01$$

$$D_a = 1.0 \text{ m}$$

$$D_c = 0.85 \text{ m}$$

$$L_a = 10 \text{ m}$$

$$L_c = 10 \text{ m}$$

$$\frac{A_2}{A_3} = 1.384$$

Relative Differences Between
Results from Discrete and Lumped-Parameter
Calculations

	Mass Flux		Vapor Mass Concentrations			Pressure	
Time s	$\frac{G_{1D}-G_{1L}}{G_{1D}}$	$\frac{G_{1D}-G_{1L}}{G_D}$	$\frac{C_{2D}-C_{2L}}{C_{2D}}$	$\frac{C_{3D}-C_{3L}}{C_{3D}}$	$\frac{C_{4D}-C_{4L}}{C_{4D}}$	$\frac{P_{2D}-P_{2L}}{P_{2D}}$	$\frac{P_{3D}-P_{4L}}{P_{3D}}$
0.0	.0015	.0015	.0002	.00025	.0001	.00001	.00001
5	.030	.011	.038	.020	.022	.0026	.0026
10	.006	.004	.019	.008	.001	.0002	.0003

Subscripts 1, 2, 3 and 4 designate axial positions
 Subscript L designates lumped-parameter model
 Subscript D designates discrete-parameter model

Figure 2.1.7 Schematic of loop test case configurations

All test cases use a two-phase, one-component, homogeneous flow model. The top plenum is a constant pressure plenum and the bottom plenum is of constant volume. The state is spatially uniform in both plena. The distributed pipe solutions are complete solutions of the homogeneous compressible flow equations (3-equation model). Compared to this distributed model the lumped formulation includes the following simplifying assumptions:

1. Compressibility effects are neglected

$$\rho = \rho(h, p_o) ; \left(\frac{\partial \rho}{\partial p} \right)_h = 0$$

- 1.1 A linearized state equation is used (to be modified later):

$$\rho = \rho_o + \frac{\partial \rho}{\partial h}_p (h - h_o)$$

2. A linear enthalpy distribution (profile) is used in each pipe.

3. The pressure term in the thermal energy equation is neglected.

Configuration 1 was applied to a free convection test case. The most important design and operating data are summarized in Table II. A fluid, initially at uniform enthalpy circulates at a low mass flow rate. At time zero heating of the left pipe with $2Q_o$ begins, while the right pipe is cooled with Q_o . Results for the mass flux and enthalpy at the exit from the heated pipe are shown in Figures 2.1.8 and 2.1.9*. The agreement between the lumped and distributed mass fluxes is good. The lumped model also reproduces the enthalpy change fairly well, except for the location of the peak and for the undershoot. This deviation is due to the assumed linear enthalpy profile, which is not a good assumption during early parts of the transients, where the actual enthalpy profile is as shown in Figure 2.1.10. The lumped energy equation predicts well the average enthalpy $\langle h \rangle$, but the assumed linear (steady state) enthalpy profile leads to a significant underprediction in h_2 at the exit of the pipe.

For the forced convection runs with two plena (configuration 2 in Figure 2.1.7) the major design and operating data are summarized in Table III. Heating and cooling rates, as well as system volumes correspond to typical PWR design data.

Figures 2.1.11 and 2.1.12 show results for a heating and cooling trip, where after an initial steady state both the heating and the cooling rates decay to zero over a time interval $\Delta t_{\text{rise}} = 1$ sec. It can be seen that the lumped parameter model compares very well with the distributed model. Only the peaks of the oscillations are slightly less pronounced in the lumped model. (Note the strongly expanded scales).

* The convergence of the distributed solutions was established by refining the nodalizations. Between the 40 and 80 node solutions the mass fluxes were within plotting accuracy and the enthalpies were equally close except at the peaks in Figure 2.1.3 where the deviation was less than 1% of h_{fg} .

TABLE II Summary of Data Used in Natural
Convection Loop Test Case

GEOMETRY:

$$V_d = 20 \text{ M}^3$$

$$L_a = L_c = 10 \text{ m}$$

$$A_a = A_c = 0.1 \text{ m}^2$$

INITIAL STATE:

$$p_o = p_d = 10 \text{ bar}$$

$$h_o = 1.5 \text{ MJ/kg}$$

$$c_{Eo} = 0.368$$

$$G_o = 3. \frac{\text{kg.}}{\text{m}^2 \text{sec}}$$

HEATING/COOLING (COMMENCING AT $t = 0$)

$$Q_o = 300 \text{ kw/m}^3 \quad ; \quad \text{started with S-ramp over rise time} \\ \Delta t_{\text{rise}} = 1 \text{ sec.}$$

TABLE III Summary of Data Used in Forced Convection Runs

$V_{do} (m^3)$	77
$V_b (m^3)$	42
$L_a = L_c (m)$	4.35
$A_a (m^2)$	10.
$A_c (m^2)$	10.
$g_a (m/sec^2)$	0.
$g_c (m/sec^2)$	0.
$Q_a (MW/m)$	-640
$Q_c (MW/m)$	+640
$\frac{f}{d_n} \left(\frac{1}{m} \right)$	1.24 & 4.96
$\Delta p_{\text{pump}} (\text{bar})$	2.0
$p_d (\text{bar})$	70
$h_o (MJ/kg)$	1.5
$\Delta t_{\text{rise}} (\text{sec})$	1.

Figure 2.1.8 Mass flux leaving heated pipe for free convection test case

Figure 2.1.9 Enthalpy at exit of heated pipe for free convection test case

Figure 2.1.10 Spatial enthalpy profile of cooled pipe at 3 sec into the transient for free convection test case

Figure 2.1.11 Mass flux leaving heated pipe in forced convection test case for simultaneous heating and cooling trip

Figure 2.1.12 Enthalpy at exit of heated pipe in forced convection test case for simultaneous heating and cooling trip

Similar results for a simultaneous trip of heating, cooling and pump head are shown in Figures 2.1.13 and 2.1.14. Again the agreement between the lumped and distributed results is very good. Even at the very small mass flow rates beyond 2 seconds the enthalpies do not differ by more than about 1% of h_{fg} . (These figures also show that for the forced convection test cases the distributed solutions with 20 and 40 nodes were almost everywhere within plotting accuracy).

For the free convection cases the lumped-parameter model required two to three orders less computer time than the distributed model. For the forced convection cases the lumped model was about six times faster than the distributed model.*

In conclusion, the feasibility of coupling lumped components in a loop describing a thermo-hydraulic system, has been demonstrated.

The algebraic reduction of the coupling equation, including differentiation of some of the algebraic equation, did not induce any numerical problems.

The lumped solutions required significantly less computer time than the distributed ones and corresponded closely to the distributed solutions.

Extensions to a few more severe transients and investigations of the blow-down case, including flow reversal (Configuration 3 in Figure 2.1.7) remain to be done. These will be completed early in April.

2.2 Component Modeling

2.2.1 Reactor Core Modeling (C. Ruger, J. Kaufman, T. Wang, W. Wulff)

The time derivative subroutine for the hydraulics is being checked out by supplying a time invariant steady-state set of boundary conditions and requiring the resulting derivatives to be zero. Difficulties have been encountered in obtaining stationary zone boundaries ($\dot{Z}_i = 0$) due to the discontinuities in vapor drift velocity at the boundaries. The drift velocity has been redefined as a continuous function, constant valued in the interior of a regime and an average of the two values associated with the regimes adjacent to the boundaries. The ordinary differential equation for Z_i , obtained from the definition of the boundary by the void propagation and vapor mass balance equations, has been written in terms of the vapor mass fraction instead of the void fraction. This equation requires the evaluation of the derivative $\partial V_{gj} / \partial \alpha$ at the boundary. Presently, this derivative is evaluated such that $\dot{Z}_i$ is zero under steady state conditions. This value of $\partial V_{gj} / \partial \alpha$ is then held fixed throughout the transients, while non-zero $\dot{Z}_i$ are computed due to variation of the remaining factors in the differential equation.

* While these numbers show the speed of the lumped parameter model, care must be used to project the potential speed of the THOR code. Neither the lumped parameter models nor the numerical integration procedures used here are identical to those considered for THOR.

Figure 2.1.13 Mass flux leaving cooled pipe in forced convection test case for simultaneous heating, cooling and pump trip

Figure 2.1.14 Enthalpy at exit of cooled pipe in forced convection test case for simultaneous heating, cooling and pump trip

It has been necessary to redevelop the expressions for the spacial dependence of the vapor mass generation Γ_v , for the continuous drift velocity formulation. The steady state distribution is assumed to represent the transient situation and is determined by the choice of liquid enthalpy distribution. Therefore, a combination of the steady mixture enthalpy and vapor mass balance equations have been used to obtain Γ_v for steady state. The average steady Γ_v was obtained by direct integration of the vapor mass balance.

Additional changes to the steady state model were required by the fact that calculation of the terminal values of mixture enthalpy in the transient routine by use of the profile function did not result in the same terminal value h_m as calculated in the steady state from the energy balance. This led to discontinuities in mass across the interfaces. This has been attributed to two sources in the modeling.

First, at the slug-annular interface a discontinuity in liquid enthalpy occurs due to the exponential approximation for h_l in the non-equilibrium slug flow regime approaching asymptotically but not exactly attaining its saturation value, h_f , at the slug-annular interface. This has now been accounted for by allowing discontinuities in vapor mass fraction and mixture enthalpy such that the energy and mass balances are satisfied across this interface. These jumps are determined in the steady state and these values used throughout the transient.

The second problem concerns the jump in properties across all the interfaces due to the use of a discontinuous pressure, which is constant in each zone. To eliminate this, a single pressure, equal to the average of the entrance and exit pressures, has been used throughout the core. Implementation of this requires an iteration procedure involving the entire steady state routine. The iteration is necessary since the exit pressure, P_N , is not given in the steady state, but the entrance mass flux is.

An initial value of P_N is obtained by the prior method of using a momentum balance across each zone. The steady state conditions are recalculated based on the average of the entrance and exit pressures. The overall steady momentum balance is then used to give a new P_N . A Newton-Raphson procedure is used to iterate on P_N with the derivatives being numerically evaluated.

For the cases tried, the procedure converges within three iterations. This process leads to automatic satisfaction of the overall momentum balance in the hydraulic transient routine for the steady state checkout problem.

Presently the hydraulic transient routine produces zero values for all derivatives for steady state conditions if the vapor drift velocity is assumed zero. A transient problem will be attempted for this condition. The inclusion of vapor drift leads to nonzero values of $\langle \dot{h} \rangle$ and $\langle \dot{c} \rangle$. This appears to be a result of the lack of knowledge of $\partial V_{gj} / \partial \alpha$ at the regime boundaries.

A logic scheme to keep track of regime boundaries is being written. The purpose is to detect disappearing and reappearing flow regimes at the core

entrance and exit and to make the appropriate adjustments in calculating derivatives. For the present time the assumption is made that the flow regimes remain in a fixed order and, therefore, it is not possible for one regime to overtake another.

For the case of a disappearing flow regime the interface position is checked at the end of each time step. When the position of an interface is calculated to be outside the fixed boundaries of the core (top or bottom) the routine backs up in time to the point where the interface position equals the fixed boundary value. The integration then continues with the time derivatives of the volume-averaged values for the non-existent flow regime equal to zero.

The same is done for a reappearing flow regime except instead of checking the interface position the boundary value of c (vapor mass fraction) is checked with the maximum allowable value. When c exceeds the maximum value the routine backs up in time to a point where c and $c_{\max}$ are equal. The integration continues with the average values of the reappearing regime set equal to the boundary values. For the reappearance of the bubbly regime and the liquid-bubbly interface the boundary enthalpy value is the criterion used.

Coding has been initiated on the coupling of the transient fuel and cladding temperature routine.

A write-up of the core model for internal communications is being prepared.

Future Plans

The existing code will be exercised under transient conditions for cases with zero vapor-drift. Investigation of the vapor drift discontinuities will be undertaken. The coupling of the thermal and hydraulic derivative routines will be coded and debugged.

2.2.2 Lumped-Parameter Model for Downcomer (U.S. Rohatgi)

This task requires developing a lumped-parameter model for the downcomer, which can predict its performance during the blowdown and refill phases. Currently a two fluid approach is being pursued. The liquid (or dense phase) and the vapor (or lighter phase) constitute two volumes separated by an interface, which is assumed to be flat. The transient formulation has been described in the last Quarterly Report (Jan. 1977). This model is simplified, so that it can be applied to experimental steady air-water data, obtained from Battelle Columbus Laboratory (Cudnik and Wooton, 1974). This simple model consists of one mass and two momentum balances in two directions for both the liquid and the gas. It is governed by eleven variables; five velocities and three pressures at the junction, two position variables for the interface and the averaged interfacial pressure. The average pressure on the interface was related to the dynamics of the liquid by an analysis of inviscid, incompressible nozzle flow. Presently, the interfacial shear stresses were neglected in the momentum balances. As there are seven equations and eleven variables, four of them are specified.

This model is applied to the data of Cudnik and Wooton (1974, Figure 16). The boundary conditions are the pressure and the gas velocity at the lower plenum

junction and the liquid velocity at the intact junction. The broken port void fraction α_2 (or corresponding upper position variable for the interface) are varied between 0.1 and 0.8. The solutions are shown in Figure 2.2.1 as plots between the fraction of the mass collected in the lower plenum (W_{e3}/W_{e1}) and the broken junction void fraction α_2 , for various values of the gas velocities. The results show correct qualitative trends. As α_2 increases for the same J_g^* , the amount of liquid going to the lower plenum increases. Furthermore, for the same α_2 , as the gas velocity (or J_g^*) increases the fraction of liquid collected decreases. The test data are also shown in the same figure and their corresponding α_2 seems to be reasonable.

With only liquid entering from the intact cold leg the downcomer model will have only three additional boundary conditions. Therefore, another relation will be needed, such as the specification that the net normal forces on both sides should act at the same point. This will be investigated next, so that α_2 can also be predicted.

2.2.3 Pressurizer (W. Wulff)

Programming of the stand-alone code for the pressurizer has been completed and some preliminary results have been obtained. The code is being executed with semiscale test conditions.

Difficulties have been encountered with the prediction of the normalized vapor mass concentration during the early (first second) portion of blowdown. As described earlier (Levine, Dec. 1976), the normalized vapor mass concentration is computed from the quasi-steady balance equations, with the vapor generation rate proportional to the time rate of depressurization $d\langle p \rangle/dt$ where $\langle p \rangle$ represents the volume-averaged pressure in the high-void zone of the pressurizer above the level of the low-void mixture. In the steady-state solution, the entire vapor produced in the low-void region is convected upward toward the dome, no vapor is stored.

The solution to the transient depressurization, however, shows that a kinematic wave moves upward through the low-void region. Above this wave the void fraction is uniform and vapor is primarily stored.

An analysis has been carried out to estimate the ratio of stored to convected vapor masses during the early time of flow development. The results will be used to predict the early vapor mass concentration.

No further work has been carried out on the pressurizer program during February and March.

2.2.4 Lumped-Parameter Model for Adiabatic Pipe (S.V. Lekach, U.S. Rohatgi and G.S. Srikantiah)

A lumped-parameter model for transient two-phase flow through constant area, adiabatic pipes has been developed. The flow field is characterized by the volume averaged, inlet and exit values of pressure p , enthalpy h , vapor concentration c and mixture mass flux G . The flow is governed by four differential equations.

Figure 2.2.1 Liquid fraction collected in lower plenum as function of broken leg void fraction for various values of J_g^* .

$$(J_g^* = [\rho_g v_g^2 / gD (\rho_l - \rho_g)]^{1/2})$$

for the balances of mixture mass, vapor mass, mixture momentum and thermal energy. These general equations were simplified by assuming that the fluid properties are based on average pressure, i.e. $\rho = \rho(\langle p \rangle, c)$. The vapor generation term in the vapor mass balance equation is approximated by a factor times its equilibrium value. This factor is zero for the frozen case and one for the equilibrium. The friction term in the mixture momentum balance is based on average quantities. The energy balance is simplified by neglecting the dissipation and vapor generation terms. These simplified equations (except for the mixture mass balance), are integrated over the space giving three ordinary differential equations in terms of volume averaged p , h , c and G . The system is not complete as the connection between inlet, exit and average values of these variables is lost in the averaging process. To complete the system of equations it is supplemented by introducing profile function, which are either estimated from experimental information or approximate steady state analysis. In the present approach the latter course is taken. The mixture mass balance, which is given here is integrated differently

$$\frac{\partial G}{\partial z} = - \frac{\partial \rho}{\partial t} = - \left(\frac{\partial \rho}{\partial \langle p \rangle} \right)_c \frac{\partial \langle p \rangle}{\partial t} - \left(\frac{\partial \rho}{\partial c} \right)_{\langle p \rangle} \frac{\partial c}{\partial t}$$

The $\partial c / \partial t$ term is replaced by use of the vapor mass balance equation, resulting in an ordinary differential equation for the mixture mass flux with axial position as the independent variable. A solution can be obtained when the profiles for p , h , c are known.

The profile for c , obtained from the steady state vapor mass balance equation, is rather complicated. However, it can be reduced to a linear function if the density is assumed to be a function of $\langle c \rangle$ and $\langle p \rangle$. This simplification is consistent with reactor plant blowdown conditions, where the vapor concentration is small at high pressures and large at low pressures. The profiles for p and h are obtained from the mixture momentum balance and caloric equation of state and are also linear. The expression for the mass flux as obtained from the ordinary differential equation is also complicated. However, if the density simplification is extended to the mixture mass balance, the solution for G is linear having a slope which is a function of $d\langle p \rangle / dt$ and $d\langle c \rangle / dt$. The formulation is completed by specifying four boundary conditions. The system of algebraic equations has been solved by Brown's method and the ordinary differential equations by Runge-Kutta method.

The accuracy of this formulation has been tested against a distributed solution, where the governing partial differential equations were linearized. These were transformed into a finite difference set of equations that resemble as much as possible the method of characteristics and was solved with an implicit technique. The finite differencing gives a block-diagonal matrix which is inverted at each time step by Riccati transformation. The scheme is always stable and the time and space stepping are determined only by the accuracy required. The optimum number of the nodes and the time step for the test cases here were established by comparing the results of one case with the solution obtained by the method of characteristics. These were applied to all the test cases to obtain the most accurate solutions.

TABLE IV.
DATA COMPARISON (SINGLE PIPE)

TIME	CM	P ₂ bar	V ₁	V _{1/2} m/s	V ₂	C ₂ ² x10 ²	h ₂ MJ/kg
1) $\tau = 0$	MOC	99.00	32.513	33.561	34.620	1.34	1.417575
	LP		32.281	33.352	34.423	1.34	1.417633
	3N		32.166	33.211	34.265	1.33	1.417572
	12N		32.483	33.529	34.585	1.33	1.417576
2) 5s	MOC	95.93	49.695	53.643	58.249	2.28	1.417036
	LP		49.278	53.544	57.798	2.28	1.417245
	5.29 3N		51.742	55.916	60.809	2.31	1.416991
	5.29 12N		52.238	55.860	61.128	2.17	1.417016
3) 10	MOC	93.91	55.526	61.434	69.035	2.89	1.416652
	LP		55.438	62.120	68.787	2.88	1.416896
	10.29 3N		55.050	61.042	69.088	2.89	1.416582
	10.29 12N		55.920	61.925	69.639	2.91	1.416635
4) 15s	MOC	92.58	57.238	64.227	73.959	3.281	1.416385
	LP		57.40	65.615	73.820	3.28	1.416634
	15.29s 3N		56.141	63.103	73.998	3.32	1.416243
	12N		57.18	64.196	74.06	3.29	1.416370

P₁ = 101 bar, c₁ = 0.007, L = 20 m, D = 1 m

Figure 2.2.2 Inlet and exit mixture velocity

Figure 2.2.3 Inlet and exit vapor mass concentration

Both these formulations were applied to homogeneous equilibrium flow for three sets of fluid conditions; high pressure (100 bar) and low concentration (.01), low pressure (40 bar) with low and high concentration (.5). For all these sets the test cases included keeping inlet p , h , c fixed and decreasing the exit pressure exponentially in time or keeping only the inlet enthalpy fixed and reducing inlet and exit pressures. The high pressure case was also solved by the method of characteristics and the results obtained from these methods are compared in Table IV. The lumped parameter solution has better accuracy than the three nodes solution. The low pressure case is described in Figure 2.2.2 and Figure 2.2.3 where both the inlet and the exit pressures are decreasing exponentially with time. The vapor mass concentration agrees well with the distributed solution. The maximum departure was in the mixture velocity, but even this was within 2.5%. However, the lumped parameter solution is improved further, if the densities occurring in the inertia terms in the mixture momentum balance equation are based on corresponding pressures (LP2). In some cases, where choking occurred, the lumped parameter solutions were in good agreement until choking occurred. For the cases where the mixture velocities are large (200 m/s), the inertia terms are significant and the terminal densities should be based on terminal conditions. However, such large velocities do not occur in reactor blowdown situations.

2.3 System Modeling (W. Wulff)

The set of equations which describe the coupling of PWR components with lumped-parameter descriptions in a single loop have been prepared for programming. The method for coupling components with discrete and lumped-parameter descriptions has been established and will be implemented, for the purpose of verification, on a test case with two pipe sections.

The single loop consists of the core, two plena, and the downcomer in the pressure vessel, the pressurizer, the U-tube steam generator primary piping, the pump, one T-branch and five pipe sections as shown in Figure 2.3.1.

There are 13 stationary interfaces between components, up to 5 moving interfaces in the core and up to one moving interface each in the lower and upper plena and one in the pressurizer or surge line.

The major findings at this time are:

1. There is only one set of equations necessary for the coupling of components with lumped-parameter descriptions, regardless of flow directions and of stagnation point positions.
2. The set of equations consists of seventy non-linear equations when used to compute transients in the unbroken loop having all pipes represented with lumped-parameter models. There are sixty-four equations when the two pipe sections which lead to the double-ended break are modeled with discrete-parameter descriptions.
3. The full set of seventy equations consists of
 36 jump conditions or interface balances for vapor mass and
 mixture enthalpy,

Figure 2.3.1 Loop system

- 11 transient mass balance equations,
- 2 steady mass balance equations for the TEE-junction and the pump,
- 7 steady momentum balances, representing the TEE-junction (2), the pump and all junctions with sudden cross-sectional area change,
- 8 out of the total of nine transient momentum balances, namely for all loop sections with inertia, except the core, and
- 6 constraints.

4. Even in the absence of cross-sectional area change, vapor mass concentration c , void fraction α , mixture enthalpy h_m and mass flux G_m are not continuous at interfaces. The mixture mass flux G_m is continuous only at stationary interfaces ($Z=0$) or at interfaces with a continuous mass fraction c . However, only one value for each property and interface is part of the set of unknowns to be computed from the set of seventy equations. The second value is substituted.

Only one set of coupling equations is required because all the possible relations between an extensive property ϕ_i (such as vapor mass fraction c or mixture enthalpy h_m) at any junction (i), its backward volume-average $\langle \phi \rangle_{i-1}$, its forward volume-average $\langle \phi \rangle_i$, and the neighboring junction properties ϕ_{i-1} and ϕ_{i+1} has been combined into a single equation. This is demonstrated below for a junction between two pipes.

Two characteristic directions are retained in the lumped-parameter models of THOR. The two directions correspond to the vapor velocity v_g and the vapor diffusion velocity $v_{gm} = v_m - v_g$. For each velocity there is defined a separate vector ζ with one component for each of the eighteen junctions between components in the hydraulic system. The i -th component of the ζ_g vector for the v_g field is defined as

$$\zeta_i = (v_g)_i / |v_g|_i \quad \text{for} \quad |v_g|_i \geq \epsilon \quad (1a)$$

$$= (\lambda_i / 2) (3 - \lambda_i^2) \quad \text{for} \quad |v_g|_i < \epsilon, \quad (1b)$$

$$\text{where} \quad \lambda_i = (v_g)_i / \epsilon \quad \text{and} \quad (2)$$

$$\epsilon = 10^{-2} \quad \text{m/s (arbitrary, tentatively)} \quad (3)$$

A corresponding definition is introduced for ζ_{gm} of the vapor diffusion velocity v_{gm} .

The variable ϕ can have two values ϕ_i^- and ϕ_i^+ at every junction (i). If the drift velocities $(v_{2i})_{i-1}$ and $(v_{2i})_i$ in the pipe sections joined at junction (i) are taken to be space-independent in each pipe, then

$$\phi_i^+ / \phi_i^- - R_i = 0 \quad (4)$$

where, for vapor mass fraction c

$$R_{ci} = G_{m,i}^- + \rho_{\ell,i} (v_{gj})_{i-1} / G_{m,i}^- + \rho_{\ell,i} (v_{gj})_i \quad (5)$$

and for mixture enthalpy h_m

$$R_{hi} = 1 + (R_{ci} - 1) c_i (h_v - h_{\ell}) / h_{m,i} \quad (6)$$

Equations 4, 5 and 6 serve to eliminate ϕ_i^- from the set of coupling equations.

The value ϕ_i^+ is governed by four profile relations, namely

(i) when $\zeta_{i-1} \geq 0$, $\zeta_i \geq 0$

$$\Psi_1 = \phi_i^+ - F_{i1} \{ \langle \phi \rangle_{i-1}, \phi_{i-1}^+, f_{\phi,i-1}(1), R_{di} \} = 0 \quad (7)$$

(ii) when $\zeta_{i-1} < 0$, $\zeta_i \geq 0$

$$\Psi_2 = \phi_i^+ - F_{i2} \{ \langle \phi \rangle_{i-1}, g_{\phi,i-1}(1), R_{di} \} = 0 \quad (8)$$

(iii) when $\zeta_i < 0$ and $\zeta_{i+1} < 0$

$$\Psi_3 = \phi_i^+ - F_{i3} \{ \langle \phi \rangle_i, \phi_{i+1}^+, f_{\phi,i}(1), R_{d,i+1} \} = 0 \quad (9)$$

(iv) when $\zeta_i < 0$ and $\zeta_{i+1} \geq 0$

$$\Psi_4 = \phi_i^+ - F_{i4} \{ \langle \phi \rangle_i, g_{\phi,i}(1), R_{d,i+1} \} = 0 \quad (10)$$

Here f_{ϕ} and g_{ϕ} are appropriate profile estimates. The simplest examples possible are obtained from linear profiles with $f_{\phi}(1) = 2$ and uniform drift velocity, $R_d = 1$

$$\Psi_1 = \phi_i^+ - \phi_{i-1}^+ + 2 \langle \phi \rangle_{i-1} = 0 \quad (7a)$$

$$\psi_2 = \phi_i^+ - \langle \phi \rangle_{i-1} = 0 \text{ (mixing)} \quad (8a)$$

$$\psi_3 = \phi_i^+ - \phi_{i+1}^+ + 2\langle \phi \rangle_i = 0 \quad (9a)$$

$$\psi_4 = \phi_i^+ - \langle \phi \rangle_i = 0 \text{ (mixing)} \quad (10a)$$

The four conditions given by Equations 7, 8, 9 and 10 are combined into one equation, Equation 11.

$$\begin{aligned} & (1+\zeta_i) \left[(1+\zeta_{i-1}) \psi_1 + (1-\zeta_{i-1}) \psi_2 \right] + \\ & (1-\zeta_i) \left[(1-\zeta_{i+1}) \psi_3 + (1+\zeta_{i+1}) \psi_4 \right] = 0 \end{aligned} \quad (11)$$

with the implication that ζ is defined for the velocity v with which the property ϕ is convected. For the special case of the simple ψ - relations in Equations 7a through 10a, Equation 11 becomes:

$$\begin{aligned} & (1+\zeta_i) (1+\zeta_{i-1}) \phi_{i-1} + 4\phi_i + \\ & (1-\zeta_i) (1-\zeta_{i+1}) \phi_{i+1} = \\ & (3-\zeta_{i+1}) (1-\zeta_i) \langle \phi \rangle_i + (3+\zeta_{i-1}) (1+\zeta_i) \langle \phi \rangle_{i-1} \end{aligned} \quad (11a)$$

Equations of the form of Equation 11 have a tridiagonal coefficient matrix in every column which has a zero element in either the upper or the lower band next to the diagonal. The 36 interface balances listed in Item 3 above are special cases of Equation 11.

The transient mass balance equations are integrals of the velocity divergence and obtained from combining the transient conservation equations for mass and thermal energy.

The unbroken loop has nine components with temporal inertia $\partial G/\partial \tau$ retained. Integration along the flow direction of the transient momentum balance gives nine ordinary differential equations of the form

$$\dot{\langle G_m \rangle} = \frac{1}{L} (p_1 - p_2) + F(\langle G_m \rangle, G_{m,1}, G_{m,2}, p_1, p_2, c_1, c_2) \quad (12)$$

Eight of these nine equations involve unknown junction pressures p_i , while the ninth equation for the core involves plena pressures which are computed from mass and energy balances. The appearance of the unknown junction pressures in Equation 12 requires:

- (i) that the eight component momentum balances are solved for eight values of $\langle G \rangle$ and six junction pressures p , simultaneously with the system balances.
- (ii) that six constraints be satisfied by six pressures involved in the eight momentum balances.

The six constraints constitute mass flux continuity at those six stationary junctions (i) between components (i-1) and (i) which have temporal inertia. The constraints imply that a pressure p_i at junction (i) and appearing in two equations of the form of Equation 12, for $\langle G \rangle_{i-1}$ and $\langle G \rangle_i$, must be computed such that, after integration of the momentum balances the results $\langle G \rangle_{i-1}$ and $\langle G \rangle_i$ produce the same mass flux at the common junction (i) when substituted into the integral of the velocity divergence, that is of

$$\frac{\partial G_m}{\partial z} - \frac{1}{\rho_m} \left(\frac{\partial \rho_m}{\partial z} \right) G_m = - \langle p \rangle \left[\left(\frac{\partial \rho_m}{\partial p} \right)_{h_m} + \frac{1}{\rho_m} \left(\frac{\partial \rho_m}{\partial h_m} \right)_p \right] - \frac{q'_w}{A \rho_m} \left(\frac{\partial \rho_m}{\partial h_m} \right)_p \quad (13)$$

The integration of Equation 13 is part of component modeling. This completes the discussion of component coupling equations. They are presently being programmed for solution by the library subprogram ZSYSTEM (IMSL, 1974).

2.4 Correlations

2.4.1 Heat Transfer Coefficients (J.M. Kaufman)

The RELAP4/MOD6 heat transfer subroutines have been successfully modified for use in THOR and are presently available as a stand-alone program. The routine divides the boiling curve into nine heat transfer modes and uses the respective correlations.

Recent comparison by EG & G with experimental data has shown that Mod 6 does not compare as well with the data as Mod 5 in the low flow, film boiling regimes.

Several modifications to Mod 6 have been suggested by EG & G for our use to improve in this area. First, 200,000 lbm/ft² hr as the criterion for changing from low flow rate correlations to high flow rate correlations can be dropped to 50,000 lbs/ft² hr. Also, instead of using the maximum of the Free Convection Plus Radiation and Dittus-Boelter Correlations, the Groeneveld and Condie-Bengston Correlations are evaluated and the larger one is compared, with the result from the Free Convection Plus Radiation Correlation. Again,

the one giving the maximum heat transfer is used. These modifications have been made.

2.4.2 Friction Factor (J.H. Jo, S. Stein)

The stand-alone program to provide two phase friction factors has been completed and debugged. This program can call either HTFS correlation (minutes of meeting on Best Estimate Models/Correlations for RELAP-4, Germantown, Maryland, February 10, 1975), or a homogeneous two phase friction model based on the Mc Adams/Isbin mixture viscosity concept (Mc Adams, et al., 1942) or the improved Baroczy routine as described in the last Quarterly Progress report. Since the HTFS correlation, in its original form, goes to infinity at $x=1$, (where x denotes quality) a linear interpolation between $x=0.9$ and the proper limit at $x=1$ was implemented. A further improvement to provide continuous derivatives at the transition can be added at short notice if any problems warrant such an addition.

2.5 Code Assembly (D.I. Garber)

The host code development, which was to provide the environment for a general modular code, has been set aside. A "hard-wired" version of THOR is being programmed as an intermediate step. Documentation in the form of a users and programmers manual has been prepared and circulated for comment.

To aid in solving the problems associated with the component coupling equations for the entire system, two service programs have been written. 1) A program to construct the structure matrix of the equation set, determine its singularity and indicate variables which are cited in only one equation has been written and is being used to study the coupling equations. 2) A code has been developed to interface the algebraic equation solver SYMBOLANG to enable easy utilization of the system. The later was developed to aid in the possible "elimination" of variables with subsequent reduction of algebraic unknowns.

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3. APPRAISAL OF ANALYSIS DEVELOPEMENT IN FOREIGN COUNTRIES

(W. J. Turner)

3.1 The NAID Code (Trimble 1976)

This code has been obtained from the Australian Atomic Energy Commission and implemented on the BNL 7600. Unfortunately, the IBM 360/65 and AAEC installation features in the code as received were much more extensive than expected, hence conversion to the 7600 was fairly complex. An informal BNL report will be issued.

The input data for RSR Test Problem 2 (which is also NEA-CSNI Standard Problem 3) was also obtained from the Australian AEC. The results give good agreement with experiment (Hancox 1976). The computation of the 12 second blowdown took 16 seconds on the BNL 7600. This compares with 15 minutes for the first 8.5 seconds with RELAP 4 at Idaho on an IBM 360/170, which is equivalent to about 110 seconds on the 7600 (Ford 1977). Thus, NAIAD is about a factor of 10 faster than RELAP on this problem.

3.2 Other Codes

To date, work has been confined to thermohydraulics codes for blowdown and/or reflood. For each of the 42 codes identified to date, Appendix 1 contains the nation and organization responsible, principle references and a brief description. Where possible, the two-phase model used is placed in one of the classes specified by Fabric (1976). The information given in Appendix 1 is not complete as in many cases further information is being sought.

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APPENDIX I

NON U.S.A. LOCA CODES

NAIAD Australian AEC (Trimble 1976) - IVSIT*

This is a blowdown code in which one-dimensional flow paths are coupled to each other and to more complex components, e.g. pumps. An implicit stable finite difference scheme (Turner 1976) is used and large time steps are possible. Both correlation and sonic discharge models can be used.

OWEN Australian AEC (Turner 1975) - IVDIT

This is a single channel one-dimensional blowdown code. An implicit stable finite difference scheme (Turner 1972) is used which allows large time steps to be taken. Correlation discharge models (Moody etc.) are used.

FIREBIRD Canada AECL Power Projects (Spinks 1976) - IVIT

This is a one-dimensional blowdown and reflood code for CANDU reactors, using Porschings (1971) numerical method. Correlation discharge models (Moody etc.) are used.

MECA Canada AECL Whiteshell (Hancox 1975) - IVIT

This is a single channel method of characteristics code. It is intended as a benchmark for other LOCA codes.

RAMA Canada AECL Whiteshell (Mather 1976) - IVIT

This one-dimensional single channel finite difference code was developed at AECL Whiteshell. Thermodynamic equilibrium and homogeneous flow are assumed. The break flow is modeled as a sonic velocity discharge. The finite difference solution method is a development of the Turner (1972) scheme.

RODFLOW Canada AECL Power Projects (Elliott 1968, Banerjee 1975) - IVSIT

This is the current AECL code for CANDU blowdown calculations. Blowdown of either a boiling or pressurized water reactor containing heavy or light water can be calculated. RODFLOW is a finite difference network code with specialized component nodes for steam drums, pumps etc. The numerical scheme is implicit in mass velocity, but explicit in mass and total energy. Moody and other correlation discharge models are available.

ANDY-CAP Denmark RISØ (Babala 1971)

This three-dimensional code for transient analysis of BWR's seems to be an ancestor of TINA. A description of the code (Babala 1971) is on request.

*one velocity, slip model, one temperature (Fabic 1976)

DANBLOW Denmark RISO (CSNI 1976 p. 17)

It is intended that this code will be used to calculate the spatial distributions of coolant, mass flow rate, enthalpy and pressure as well as fuel rod temperature, cladding deformation and rupture in a P(B)WR core during the blow-down phase of a LOCA. The hydraulic model which is based on the subchannel approach includes slip, countercurrent flow and thermodynamic non-equilibrium. This hydraulic model is being tested in the TINA code.

NORCOOL Denmark RISO (Andersen, J. G. M. 1976)

This is a BWR reflood program in which the physical processes occurring during reflood are modeled in considerable detail. Separate equations for liquid drops, liquid films, steam and liquid water are solved in the hydraulic simulation. All modes of heat transfer are included.

REMI/HEAT-COOL Denmark RISO

This code is being developed as part of the NORHAV project (CSNI 1976 p. 3). There appears to be no difference between it and NORCOOL. The first version of the code was written by Andersen (1973).

TINA Denmark RISO (Bech 1974, 1976)

TINA is a subchannel code for the core of a nuclear reactor or test loop. Liquid non-equilibrium is allowed by means of two mass equations, and homogeneous flow is assumed. Neither superheat nor condensation are allowed.

RISQUE Denmark RISO (Andersen, M. B. 1976, Eget 1977) - 2V2T

A one-dimensional single channel or loop can be simulated with this code. A two fluid six equation model is used and the solution is found by means of a linearised implicit finite difference scheme.

TPD3 Denmark RISO (Andersen 1975) - IVD2T

This is a single channel blowdown code in which a four equation saturated steam drift flux model is used. The solution is obtained by means of a finite difference method.

CERES France CEA (Gomolinski 1974)

This code is designed for computing the hydraulic behavior of a PWR during reflood. FLECHT correlations are used for the fuel to coolant heat transfer. The core inlet and outlet conditions are used in the code FLIRA for detailed analysis of the fuel behavior.

CLYSTERE France EDF (Bauer 1976) - IV2T

This PWR blowdown and reflood code is being developed by Electricité de France. Detailed descriptions of most components are included. Nonlinear implicit finite difference schemes are used for the partial and ordinary differential equations.

DABBOUS France CEA (Menessiev 1975) - IVIT

This is a simplified version of DANAIDES and uses a three equation reservoir and connection formulation. Homogeneous flow is assumed and Runge-Kutta integration is used.

DANAIDES France CEA (Gomolinski 1974) - IVIT

This blowdown code uses a reservoir and connection formulation. A Moody discharge model is used and Runge-Kutta integration is performed.

DULCINEE France (Dutraive 1970)

This code simulates the behaviour of a water-cooled reactor at low pressure during a reactivity accident or a LOCA.

FLICA France CEA (Fajeau 1969)

This is a transient two-phase flow code in which unequal phase velocities related by a steady state correlation are allowed.

FLIRA France CEA (Gomolinski 1974, Chabrillac 1976)

This single channel PWR reflood code includes detailed calculation of the quench front motion. A four equation model with two energy equations is used. Either the liquid or vapor is saturated. The channel boundary conditions must be obtained from elsewhere.

IALEMOS France CEA (Chabrillac 1976)

The method of characteristics is used in this code. The five equations which specify the two-phase flow model, are the 3 conservation equations plus equations for liquid non-equilibrium and vaporization delay.

VIBRAPHONE France CEA (Lepareux 1975)

This is a 3D code in which the pressure distribution during the first milliseconds of blowdown is determined. More information is being obtained.

BRUCH Germany LRA (Karwat 1974) - IVIT

Bruch uses a three equation reservoir and connection formulation. It has fixed geometry thus different versions are used for different reactor types and experiments; BRUCH-D for PWR, BRUCH-S for BWR, BRUJET for BWR with jet pump etc. The equation of state for liquid is modified to reduce the sonic speed; also the equation of state fits are not good below about 5 bar (Winkler 1976).

BLAST Germany LRA

This is an early version of DAPSY.

DRUFAN Germany LRA (Wolfert 1976) - IV2T

In this code a reservoir and connection formulation is used with a mass equation for each phase. This allows liquid non-equilibrium, the vapor phase being assumed saturated. Both Moody and sonic discharge models are available.

DAPSY Germany LRA (CSNI 1975 p. 99)

This is a method of characteristics code for the investigation of pressure wave propagation phenomena in pressurized and boiling water reactors.

NOWRA Germany LRA (CSNI 1975 p. 209)

Work was terminated on this reflood code when it was decided "that further development ... should be concentrated on a different approach"

WAK-1 Germany KWU (CSNI 1976 p. 187)

This is a refill code. More information is on request.

KAPCOR Euratom (Fritz 1973) - IVSIT

KAPCOR uses a reservoir and connection formulation. Liquid is assumed incompressible in all nodes other than the inlet plenum. A detailed fuel model and either Moody or Zalonek discharge models are included.

NICKY Euratom (Forti 1974)

In this code the system pressure is obtained by solving the momentum equation integrated over the entire system excluding the break. The method is closely related to the momentum integral method (Meyer 1961). Thermodynamic equilibrium is assumed and slip between the phases allowed. A Moody discharge model is used.

DOLCE Italy CISE (CSNI 1976 p. 118)

The onset of CHF under LOCA conditions is calculated in this code. Details of the hydraulic calculation are on request.

RATT Italy CISE (Martini 1976) - IVSIT

This is a single channel blowdown code. Both correlation and sonic discharge models are available. A stable finite difference scheme (Turner 1972) is used.

TILT Italy CISE (Magni 1973)

The mean system pressure as a function of time is required input data for this code. Development of TILT has ceased and the effort has been transferred to the RATT code.

ALARM-B1 Japan JAERI (Shimooke 1975) - IVIT

Only the hydraulics are simulated in this BWR code. A volume and connection formulation is used. The FLASH bubble rise model is used. The momentum flux term is omitted from the momentum equation. The output from this code provides core boundary conditions for HYDY.

ALARM-B2 Japan JAERI (Shimooke 1975) - IVIT

A fuel model (Asahi 1976) and point neutron kinetics are included. An average and a hot channel are simulated.

ALARM-P1 Japan JAERI (Akimoto 1976) - IVIT

This version of ALARM is for BWR's. The numerical solution is obtained by a simple forward explicit method. The momentum flux term is included. The FLASH bubble rise model is used. Pump, fuel model and neutron kinetics are included.

ALARM-P2 Japan JAERI (Kobayashi 1975)

A two-dimensional method of characteristics solution of the downcomer and improved numerical methods are planned.

DEPCO Japan JAERI (Sato 1975)

Very fast transients following a break are simulated by solving the three conservation equations for homogeneous thermodynamic equilibrium flow by the method of characteristics.

REFLA-1 Japan JAERI (CSNI 1975 p. 223)

This reflood code is based on a three flow regime model. It was completed in September 1974. Further information is on request.

SENHOR Japan JAERI

This code was developed for analyzing pipe breaks in the pressure tube reactor FUGEN. Version II is for large breaks (Kawabe 1975) and version IV for small breaks (Minato 1976). The momentum equation for individual coolant channels is treated in a quasi steady fashion, and solved for the mean channel pressure. The discharge flow is calculated from the conditions for zero characteristic velocity.

THALES-P Japan JAERI (Shimooke 1975, Sato 1975)

This code is intended for accident analysis of small breaks in PWR. Advanced two-phase models such as the drift flux model will be incorporated.

THYDE-B Japan JAERI (Shimooke 1975)

This code is similar to THALES and is intended for small breaks in BWR.

NORA Norway (CSNI 1976)

This is a ID code for calculation of pressure, temperature and mass flow transients in a mixture of air, steam and water in a pipe network. Critical flow is calculated from the zero characteristic velocity. Point neutron kinetics, water levels and accumulators are included.

RELAP-UK United Kingdom UKAEA (Brittain 1976) - IVDIT

This code was developed from RELAP-3. The changes are designed to improve modeling of the UK SGHW reactor design, to automate the steady solution and to reduce computing time.

TREACLE-TOFFEE United Kingdom UKAEA (Cowking 1966) - IVIT

This code is a reservoir and connection, 3 equation, fixed geometry code for LOCA in an SGHW reactor. 54 reservoirs and 69 connections were used to model the system.

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