REACTOR SAFETY RESEARCH PROGRAMS

QUARTERLY PROGRESS REPORT

APRIL 1 - JUNE 30, 1977

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APRIL 1 - JUNE 30, 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 third of a series of quarterly reports which combines the reports of the individual projects mentioned above. The previous reports BNL-NUREG-50624 and BNL-NUREG-50661, covered the periods October 1 through March 31, 1977.

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I. HTGR SAFETY EVALUATION

SUMMARY

Several experiments have been completed in the thermochromatographic apparatus on the volatilization and stability of CsI under HTGR conditions. The results indicate that CsI is stable at temperatures up to 800°C in an inert or slightly oxidizing environment. In the presence of oxygen, decomposition occurs with the release of elemental iodine.

A comparison has been made of diffusion coefficient values for the transport of cesium in graphite used in the SORS code with values determined in recent experimental studies. Since the SORS code values are found to be conservative, our experimental program on cesium transport in graphite will be terminated. Only work on cesium release from a mixed cesium-strontium adsorbate is continuing.

The effect of mean tensile stresses on the high-cycle fatigue have been investigated for solution-annealed Type 304 stainless steel. For small mean stresses, there appears to be a slight increase in fatigue strength at 1100°F but, at higher mean stress levels, significant strength losses are noted. Goodman's analysis cannot be applied at high temperatures and high stress levels since it does not take into account the effects of creep deformation. An empirical relationship was, therefore, developed based on the time to failure and the rupture strength. This gives a much more reasonable estimation of the effects of mean tensile stresses on fatigue life for conditions where creep is present.

High-cycle fatigue strengths of Type 304 stainless steel are increased by thermal aging prior to test, but the effect appears to be transitory since increased aging at 1000° and 1200°F, for times of up to 9000 hours, reverse the hardening trend. For Incoloy 800H aging caused only strengthening for the conditions evaluated and, for normalized and tempered 2½Cr-1Mo steel, only weakening was noted.

Creep-rupture testing was continued on Incoloy 800H and Hastelloy H. One preliminary run was carried out in a helium environment supplied by the Materials Test Loop (MTL).

A comparison was made of the fracture behavior of PGX graphite which had been oxidized in the Helium Impurities Loop (HIL). The fracture path was shown to be associated with microstructural fractures in the material. This is in contrast to unoxidized graphite which showed far less tendency to follow the underlying structure.

The construction of the MTL has been completed and shakedown testing has begun. One of the creep testing machines was connected to the loop temporarily and a test specimen exposed to hot helium while under stress. Various designs for an automatic control system to maintain specified levels of impurities are being studied.

The size of the iron clusters in PGX graphite and their chemical form in various gaseous environments were examined by high-temperature (in situ) Mössbauer spectroscopy. Characterization of the pore structure of PGX graphite was initiated by a permeability study with He at temperatures up to 800°C. The study of the oxidation kinetics of PGX graphite by H₂O/H₂ was continued with determinations being made on samples with different iron concentrations.

Cesium iodide appears to react with oxygen and with nitrogen. Current interest is directed towards determining whether the reaction with nitrogen inhibits the evolution of iodine in the reaction with oxygen.

Various experiments that recorded the spontaneous buildup of impurities in the HIL during operating conditions were conducted. The loop was subsequently cleansed with oxygen and then with hydrogen. The buildup of impurities is now judged to be sufficiently slow so as not to interfere with the carbon monoxide injection experiments to be performed during the next quarter.

The CO and CO₂ monitors for the HIL have undergone a range change (0-5%) to acquire high impurity level data in loop experiments.

Construction of the mass spectrometer is continuing. The instrument has been under vacuum and has produced residual gas spectra. In order to eliminate the need for a liquid nitrogen cold trap, the use of a perfluorinated diffusion pump oil is being investigated.

The SODEMME code has been extended slightly and the subroutine HTRNSFR has been simplified considerably. A sample problem has been run that simulates a 3000 MWth HTGR during core heatup. A topical report describing the theory and use of the code and which contains an illustrative example will be published during the next quarter.

The code EVAPI has been written to model the evaporation of fission products in the core under the conditions of a loss of forced cooling heat-up accident.

A series of experiments to test the assumptions and predictions of the codes HAZARD and CIRC in the analysis of gas layering in the containment building following a depressurization accident is being planned.

The surveillance of the German Federal Republic's HTR Program was formalized during the quarter. One facet of this effort involves the generation of a computerized file of technical reports issued by various German organizations and sent to BNL under information exchange agreements. Another facet, related to keeping informed on current developments in the German Program, involves the review of several news services that specialize in the nuclear industry.

The damping ratio of insulation cover plates was measured by analysis of the decay of vibrations excited by impulsive forces under a variety of support conditions. The support conditions selected simulate failed attachment fixtures and degradation of the Kaowool insulation.

A report providing basic concepts and a review of the problem areas associated

with the development of analytical and experimental programs for a systematic evaluation and comparison of the currently available fracture mechanics theories was issued.

The vibrations testing of the vertical array test rig continued with an extension to investigate the effects of simultaneous horizontal and vertical excitation. To perform the tests, some modifications of the test rig were made and a second shaker, a mechanical machine, was employed. The preliminary design of a test rig suitable for the vibrations testing of a three-dimensional block array has been completed.

The elastic phase of the steam generator tube weld evaluations is near completion. The most severely stressed location was found to be the fusion line between the I-800 and I-82 of the I-800/I-82/2 $\frac{1}{2}$ Cr - 1 Mo weldment. The investigations of crack severity using LEFM techniques indicated that for the normal conditions of loading there is not enough energy at the crack tips to promote propagation.

1. Fission Product Release and Transport

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

Several experiments have been completed in the thermochromatographic apparatus on the volatilization of CsI tagged with ^{137}Cs and ^{131}I . In Figure 1.1 are shown the results of an experiment (Run 2) in which CsI (approximately 15 mg) was volatilized at 800°C inside a closed graphite crucible. The crucible was placed inside a Type 304 stainless steel tube through which purified helium gas flowed at the rate of 50 cc STP/min. Volatilization of the CsI out of the graphite crucible was essentially complete in about 30 minutes. It is observed in Figure 1.1 that cesium and iodine deposition occurred together on the wall of the steel tube at a temperature of 600°C . It is evident from the fact that the deposition of the two elements, Cs and I, occurred at the same temperature that CsI is stable in a helium environment, in the presence of graphite, at 800°C .

The results of a second experiment, Run 4, are shown in Figure 1.2. The experimental conditions employed were the same as for Run 2 with the exception that the gaseous environment was helium containing 0.6% by volume of water vapor. Cesium and iodine deposition occurred on the steel tube at a temperature of 580°C , which is very similar to the deposition temperature of 600°C for Run 2. CsI is, thus, stable in a slightly oxidizing atmosphere as well as in an inert atmosphere.

An additional experiment was performed, Run 3a in Figure 1.3, in which CsI was volatilized from an open alumina crucible at 800°C in helium containing 0.6% water vapor. The primary differences between this experiment and Run 4 were the absence of graphite and the rapid release of CsI from the open alumina crucible. Release was complete in 10 minutes instead of 30 minutes. It is observed in Figure 1.3, Run 3a, that the results of this experiment are identical to those of Run 4.

It was of interest to determine the effect of a stronger oxidizing environment on the CsI deposited on the steel tube. Helium gas containing 10 volume percent oxygen was flowed over the deposit at 580°C at a rate of 50 cc STP/min. It is observed in Figure 1.3, Run 3b, that CsI reacted with the oxygen releasing elemental iodine which deposited on the steel tube at room temperature and in a charcoal trap at liquid nitrogen temperature. The cesium-bearing deposit remaining on the steel tube at 540°C is probably an oxide of cesium.

In order to determine the nature and extent of the interaction of the cesium oxide deposit with the steel tube, the deposit was heated to 800°C and the release of cesium was monitored. It is observed in Figure 1.4, Run 3c, that approximately 70% of the cesium was readily released with the remaining cesium tightly held on the stainless steel tube. The volatilized cesium deposited at a temperature of about 460°C , as is observed in Figure 1.5.

We may conclude from the experimental results that cesium iodide is stable under HTGR conditions. If fission product cesium and iodine combine in the fuel

Figure 1.1. Deposition of CsI volatilized from a closed graphite crucible in helium.

Figure 1.2. Deposition of CsI volatilized from a closed graphite crucible in helium containing 0.6 volume percent water vapor.

Figure 1.3. (Run 3a) deposition of CsI volatilized from an open alumina crucible in helium containing 0.6 volume percent water vapor. (Run 3b) redistribution of the cesium and iodine deposited on the steel tube in helium containing 10 volume percent oxygen.

Figure 1.4. Loss of cesium deposited on steel tube upon heating to 800°C in helium.

Figure 1.5. Redistribution of cesium after heating deposit to 800°C in helium.

particles to form CsI, this molecular species will not decompose on contact with graphite or stainless steel. These experiments indicate that the thermochromatographic technique can yield useful information about the interaction of volatile fission products with HTGR materials.

1.2 Cesium Transport in H-451 Graphite (S. Aronson, J. Mittelman - Brooklyn College)

Two studies have recently been published on the diffusion of cesium in graphite (Chandra, 1976 and McEnaney, 1976). These investigators have obtained values for diffusion coefficients and have made comparisons with previous work. The values of the diffusion coefficients can be compared with those used in the SORS code for fission product release (Schwartz, 1974) which are based on the semi-empirical treatment of Norman, 1974. These data can also be compared to diffusion coefficients calculated from our data on cesium release from H451 graphite (Aronson, 1976 and Aronson, 1977).

A comparison of the diffusion coefficients at 950°K determined in the various studies is shown in Table 1.1. The term "effective" diffusion coefficient is used in the table because the diffusion process is complex involving at least two transport mechanisms. Nevertheless, effective diffusion coefficients useful in predicting cesium release rates from graphite can be calculated on the assumption that a single bulk diffusion process is operative.

TABLE 1.1

Comparison of Effective Diffusion Coefficients for Cesium in Graphite at 950°K

<u>Reference</u>	<u>Diffusion Model</u>	<u>Effective Diffusion Coefficient (cm^2/sec)</u>
SORS code	Semi-empirical	10^{-5}
Present study	Sphere with uniform initial concentration	10^{-6}
Chandra, 1976	Hollow cylinder, source on inner surface	10^{-8}
McEnaney, 1976	Sphere with uniform initial concentration	10^{-8}

It is observed in Table 1.1 that the diffusion coefficients calculated by Chandra, 1976 and McEnaney, 1976 are in good agreement. Our values are two orders of magnitude higher and the SORS code value is three orders of magnitude higher. It should be noted that the three experimental studies were made with different structural graphites, different concentrations of cesium and different experimental conditions. It should further be noted that our experimental proce-

dure was not developed with the intention of determining precise diffusion coefficients but to obtain survey information on cesium release and to compare release rates in inert and oxidizing atmospheres.

A general outline of a mechanism of cesium transport in graphite has been developed, through the studies cited above and previous investigations, which can be used to calculate data in semiquantitative agreement with experimental observations. The SORS code values for the diffusion coefficient are conservative from the point of view of HTGR safety in that calculated release rates are higher than predicted from the experimental studies. It is, therefore, evident that additional work on cesium transport in unirradiated graphite is not required. Our program on cesium release from graphite will therefore be terminated.

The final phase of our investigation of cesium release from graphite will be a study of a mixed adsorbate of cesium and strontium. For these experiments, graphite samples will be soaked in a solution of $\text{Sr}(\text{OH})_2$ and CsOH and will then be dried and heated to decompose the hydroxides. Hydroxides of the metals will be used rather than the nitrates because it is anticipated that they will decompose more readily upon heating.

In connection with this study, several samples of H451 graphite were doped with CsOH tagged with ^{134}Cs . Release of cesium from graphite was measured. In one experiment, Run 6-1 in Figure 1.6, cesium release was followed in vacuo up to a temperature of 777°C . The initial adsorbate concentration was 0.148 mg Cs/g C . The initial count rate was 108 counts/sec . Loss of cesium started to occur below 400°C in contrast to the behavior of CsNO_3 where loss begins above 500°C . After this run and several others in helium and water vapor were completed, it was realized that alkali hydroxides readily absorb CO_2 from air at room temperature to form alkali carbonates. The adsorbate in these samples may, therefore, have been Cs_2CO_3 rather than CsOH . Additional samples of CsOH will be prepared which will be stored in an air-free environment.

A second group of graphite samples was doped with Cs_2CO_3 . Two runs on Cs_2CO_3 -doped graphite are shown in Figure 1.7. In Run 7-1, the initial adsorbate concentration was 154 mg Cs/g C and the initial count rate was 52 counts/sec . In Run 7-2, the initial adsorbate concentration was 151 mg Cs/g C and the initial count rate was 58 counts/sec . In Run 7-1, cesium release was followed in vacuo to 691°C . In Run 7-2, release was followed in vacuo to 408°C and then in helium to 680°C . A significantly lower rate of release in the temperature range between 408° and 521° was observed for Run 7-2 than for Run 7-1. The reason for this may be the more rapid volatilization of Cs_2CO_3 in vacuo than in helium as was the case for CsI (Aronson, 1976). It is of interest to compare Run 7-1 for Cs_2CO_3 in vacuo with Run 6-1 for CsOH in vacuo. Visual comparison indicates that the data for the two runs are quite similar which supports our suggestion that the CsOH sample may have converted to Cs_2CO_3 before the run.

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Figure 1.6. Release of cesium in CsOH-doped graphite sample in vacuo.

Figure 1.7. Release of cesium in Cs_2CO_3 -doped graphite samples in vacuo (Run 7-1) and in vacuo and then helium (Run 7-2).

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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, however, 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) has been 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 5 MTS fatigue units are operating routinely with emphasis being placed on high-cycle fatigue testing in air. Routine maintenance was recently completed on the hydraulic system to replace hoses which were showing signs of cracking. The 8 creep units are operating normally for in-air testing and a purchase order was recently placed for 8 additional units to increase the testing capacity. A contract for the purchase of an additional fatigue machine and various accessories is currently being negotiated.

(a) Mean Stress Effects on High-Cycle Fatigue

An assessment has been made of the effects of mean tensile stresses on the high-cycle fatigue of solution-treated Type 304 stainless steel, normalized and tempered 2½Cr-1Mo steel, and Incoloy-800H. The data reported here will be focused on the Type 304 stainless steel because they are the more comprehensive.

Testing was conducted in air at a cycling rate of 40 Hz for temperatures of 72 and 1100°F (Table 2.1). At the lower temperature, where creep is absent, the limited data conform to the predictions of the Goodman law as shown in Figure 2.1. In this figure the alternating stress range is plotted versus the mean stress. By drawing the maximum and minimum stress lines through the ultimate tensile stress (UTS) and the fatigue endurance limit value for fully-reversible deformation, as shown, a triangular area is defined within which fatigue failure should not occur. The data obtained are in accordance with this Goodman criterion (Dieter, 1961).

At 1100°F, however, significant creep deformation will occur and a criterion based on the UTS value would not be expected to apply. The data in Figure 2.2

Table 2.1

High-Cycle Mean-Stress Fatigue Data for Solution-Treated Type 304 Stainless Steel

Spec. No.	Test Temp. (°F)	Stress (ksi)		Cycles to Failure (N_f) ⁽¹⁾	Final Spec. Length (in) ⁽²⁾	Final Spec. Diam. (in)	Orig. Spec. Diam. (in)
		Mean	Cyclic				
MS1-83	72	5.5	27.5	$>7.2 \times 10^7$	2.630	0.1240	0.1251
MS1-82	72	6.0	30.0	2.7×10^5	2.674		
MS1-87	72	6.0	30.0	$>6.6 \times 10^6$	2.633	0.1245	0.1249
MS1-86	72	6.25	31.2	1.3×10^5	2.675		
MS1-85	72	6.75	32.7	6.1×10^4	2.671		
MS1-74	72	7.5	37.5	2.3×10^4	2.695		
MS1-98	1100	6.0	22.0	$>1.0 \times 10^8$	2.664	0.1175	0.1250
MS1-89	1100	6.0	22.0	$>7.2 \times 10^7$	2.636	0.1235	0.1251
MS1-96	1100	6.0	22.8	9.3×10^4	2.683		
MS1-97	1100	6.0	22.8	9.3×10^4	2.690		
MS1-95	1100	6.0	24.0	6.8×10^4	2.679		
MS1-94	1100	6.0	26.0	2.0×10^4	2.680		
MS1-93	1100	8.0	20.0	$>1.0 \times 10^8$	2.660	0.1190	0.1251
MS1-92	1100	8.0	21.6	4.0×10^5	2.695		
MS1-90	1100	8.0	22.0	3.8×10^5	2.690		
MS1-91	1100	8.0	22.8	7.9×10^5	2.685		
MS1-88	1100	8.0	24.0	4.8×10^4	2.695		
MS1-111	1100	12.0	18.0	$>1.2 \times 10^8$	2.638	0.1235	0.1260
MS1-113	1100	16.0	14.0	$>1.2 \times 10^8$			
MS1-112	1100	16.0	16.0	2.4×10^7	2.730		
MS1-137	1100	16.0	19.0	1.2×10^7	2.703		

Table 2.1 - Cont'd.

Spec. No.	Test Temp. (°F)	Stress (ksi)		Cycles to Failure (N_f) ⁽¹⁾	Final Spec. Length (in) ⁽²⁾	Final Spec. Diam. (in)	Orig. Spec. Diam. (in)
		Mean	Cyclic				
MS1-129	1100	20.0	10.0	$>1.7 \times 10^7$	2.650	0.1170	0.1245
MS1-132	1100	25.0	8.0	4.4×10^7	2.690		
MS1-131	1100	25.0	10.0	1.9×10^7	2.695		
MS1-133	1100	25.0	12.0	4.2×10^7	2.690		
MS1-136	1100	25.0	18.0	1.1×10^6	2.715		

Notes: (1) Cyclic frequency is 40 Hz.

(2) The original specimen length is 2.625

GOODMAN DIAGRAM FOR SOLUTION-TREATED
TYPE 304 STAINLESS STEEL AT 72°F.

Figure 2.1

EFFECT OF MEAN STRESS ON THE FATIGUE OF
SOLUTION-TREATED TYPE 304 STAINLESS STEEL.

Figure 2.2

show, in fact, that low mean stresses (σ_m) seem to be beneficial to fatigue strength. At higher mean-stress levels, where significantly more creep occurs, the fatigue levels are greatly reduced. For the limiting case in Figure 2.2 where the alternating stress, σ_a , is zero, failure times were obtained from the stress-rupture tests. It is obvious that as the alternating stress decreases, and the mean tensile stress increases, there is a tendency to move from fatigue-controlled to creep-controlled failure.

In Figure 2.3 is plotted a Goodman diagram for the 1100°F data. Many specimens failed within the "safe" region which shows this analysis has serious limitations as a conservative procedure for predicting mean-stress effects on fatigue. It was, therefore, decided to investigate a different approach based on times-to-failure, t_f , rather than the fail/no-fail criteria used by Goodman. The data in Figure 2.2 were replotted in the form of "isochronous mean-stress fatigue curves" (Figure 2.4). These were obtained by drawing "vertical" lines through the 10, 10^2 , 10^3 h etc. points and determining the combinations of σ_a and σ_m which give failure in a given time. All of the points along the mean-stress axis in Figure 2.4 were obtained from stress-rupture data on the same heat of material. It was found that, to a good approximation, the mean and alternating stresses for a given t_f were related by a simple cubic expression, as shown in Figure 2.5, viz.:

$$\sigma_a = \sigma_o - B\sigma_m^3 \quad (2.1)$$

B is a material constant and σ_o is the fatigue strength for the zero mean-stress condition. For the t_f range evaluated, σ_o is a constant, equal to 21.5 ksi. The significance of B may be deduced from the relationships in Figure 2.5. For a given time-to-failure, t_f ,

$$\sigma_a = \left[\sigma_o - B\sigma_m^3 \right]_{t_f} \quad (2.2)$$

For the boundary condition $\sigma_a = 0$, the mean stress will be equal to the creep-rupture stress value σ_r . Hence, from Eq. (2.2):

$$0 = \sigma_o - B\sigma_r^3 \quad (2.3)$$

or

$$B = \sigma_o / \sigma_r^3 \quad (2.4)$$

From Eqs. (2.1) and (2.4):

$$\sigma_a = \sigma_o \left[1 - \left(\frac{\sigma_m}{\sigma_r} \right)^3 \right]_{t_f} \quad (2.5)$$

This expression takes time-dependent deformation into consideration through the rupture strength term σ_r . A knowledge of the fatigue limit, σ_o , for fully-

ISOCHRONOUS MEAN STRESS FATIGUE CURVES FOR
 SOLUTION-TREATED TYPE 304 STAINLESS STEEL
 AT 1100°F.

Figure 2.4

RELATIONSHIP BETWEEN THE ALTERNATING AND
MEAN STRESSES FOR THE HIGH-CYCLE FATIGUE
OF TYPE 304 STAINLESS STEEL AT 1100° F.

Figure 2.5

reversible deformation, and the rupture strength for a given t_f will, therefore, allow the isochronous mean-stress fatigue curve to be calculated,

To check this equation, values of the rupture strength for 10, 10^2 , 10^3 h, etc., were taken from the stress-rupture data and calculated isochronous curves were obtained. A comparison with experimental curves is given in Figure 2.6, and good agreement is shown.

Eq. (2.5) may be rewritten in the form:

$$\left(\frac{\sigma_a}{\sigma_o} \right) + \left(\frac{\sigma_m}{\sigma_r} \right)^3 = 1 \quad (2.6)$$

In this form the damage components due to fatigue and creep are separated and shown to total up to unity for a given t_f value. Eq. (2.6) represents a special case of the creep-fatigue interaction since it is applicable to cases where creep and fatigue damage is allowed to occur simultaneously. In much of the work described in the open literature the test conditions are adjusted so that creep and fatigue increments are applied alternately.

For cyclic frequencies other than the 40 Hz used in this work, a different set of isochronous curves would be obtained because of the change in the relationship between t_f and N_f .

Also shown in Figure 2.4 are the various regions where failure is controlled by creep, fatigue, and a combination of creep and fatigue. These were defined from detailed metallographic studies.

During fully-reversible fatigue, changes in specimen length are not usually expected unless there is a substantial difference between tensile and compressive flow, such as for the Bauschinger effect (Dieter, 1961). In Table 2.2 are summarized the results of measurements made on a variety of samples tested at BNL for the fully-reversible condition. Based on changes in specimen length and diameter, $2\frac{1}{2}$ Cr-1Mo steel and Type 304 stainless steel do not appear to show any strain accumulation. In the case of Incoloy-800, however, there is an indication that strain accumulation does occur for fully-reversible fatigue. At this time the reasons for this are obscure, and further work is needed to determine whether the strain continuously increases with time, or whether it is associated mainly with the initial stages of deformation prior to the establishment of more stable hysteresis loop conditions.

For applied mean-stress situations a net increase in specimen length would be expected at high temperatures, where creep and fatigue occur simultaneously. The actual amount of strain accumulation should be dependent on the ratio σ_a/σ_m . In the case of failed specimens, actual strain measurements based on the reduction in cross-sectional area usually could not be made because of severe deformation near the fracture zone. It was, therefore, decided to examine the unfailed samples for which accurate estimates of strain accumulation may be made based on the reductions in cross-sectional area. Such measurements would not be complicated by severe distortion around a fracture zone or by the presence

COMPARISON OF CALCULATED AND EXPERIMENTAL
ISOCHRONOUS MEAN-STRESS HIGH-CYCLE FATIGUE
CURVES FOR SOLUTION-TREATED TYPE 304 STAINLESS
STEEL AT 1100°F.

Figure 2.6

Table 2.2

Dimensional Changes in Structural Materials During High-Cycle,
Load-Controlled, Fully-Reversible Fatigue Testing

<u>Spec. No.</u>	<u>Heat Number</u>	<u>Test Temp. (°F)</u>	<u>Cyclic Stress (ksi)</u>	<u>Accum. Cycles⁽¹⁾</u>	<u>Final Spec. Length (in)</u>	<u>Orig. Spec. Length (in)⁽²⁾</u>	<u>Final Spec. Diam. (in)</u>	<u>Orig. Spec. Diam. (in)</u>
<u>2½Cr-1Mo Steel (0.120% C, N & T)</u>								
MC1-24	-	1000	30.0	5.8×10^7	2.626	2.625	0.124	0.126
<u>Solution-Annealed Type 304 Stainless Steel</u>								
MS1-109	55697	72	17.5	1.1×10^7	2.631	2.625	0.125	0.125
MS1-60	55697	800	20.6	1.5×10^8	2.622	2.625	0.127	0.126
MS1-42	55697	1100	20.4	1.7×10^8	2.631	2.625	0.125	N/M ⁽³⁾
WB-3 ⁽⁴⁾	BNL Ht.	1100	24.0	6.2×10^7	2.625	2.625	0.125	N/M
WB-5 ⁽⁵⁾	BNL Ht.	1100	24.0	1.4×10^8	2.603		0.125	N/M
<u>Incoloy-800 and Incoloy-800H</u>								
MN1-127	HH5556A	1000	33.0	2.0×10^7	2.625	2.625	0.125	0.126
MN1-130	HH5556A	1000	34.5	1.7×10^7	2.623	2.625	0.124	0.126
MN1-50	HH7531A	1100	20.0	1.3×10^8	2.640	2.625	0.120	N/M
MN1-53	HH7531A	1100	20.0	1.8×10^7	2.636	2.625	0.124	N/M
MN1-41	HH3113A	1200	25.0	1.9×10^7	2.635	2.625	0.124	0.125
MN1-42	HH3113A	1200	26.0	7.8×10^7	2.625	2.625	0.123	0.125
MN1-65	HH7531A	1200	26.4	1.4×10^8	2.627	2.625	0.126	0.126

- Notes: (1) Cyclic frequency is 40 Hz except for specimen MS1-109 which was cycled at 5 Hz.
(2) Nominal value for all machined samples.
(3) Values not measured.
(4) Thermally aged 1000°F/2100 h prior to testing.
(5) Thermally aged 1000°F/5000 h prior to testing.

of wide fatigue/creep cracks which are frequently observed after failure. The data in Table 2.3 show that significant strain accumulation occurs for both Type 304 stainless steel and Incoloy-800. Although the data are limited it is probable that strain accumulation is encouraged by:

- . increasing temperature
- . accumulated fatigue cycles (test duration)
- . increasing mean stress
- . increasing alternating stress.

These factors must, therefore, be considered in detail if mean-stress effects are to be incorporated into component design and analysis.

The detrimental effects of mean stress on fatigue life may be considered from a different standpoint - that of stress-rupture life. In Figure 2.7 are plotted rupture times for specimens which are subjected to various combinations of σ_a and σ_m , including the case for pure stress-rupture where σ_a is zero. Extremely large reductions in rupture time are noted for cyclically-stressed specimens, even though the maximum stress is frequently very much less than the steady state stress level for the standard stress-rupture tests. For the Type 304 stainless steel tests it is seen that even small cyclic stresses of 8-10 ksi decrease the rupture time by a factor of about 3 at 1100°F. There is, obviously, a need for more definitive data on the effects of small high-frequency cyclic stresses on the creep and stress-rupture behavior of reactor materials. A more complete description of this work is given elsewhere (Soo, 1977).

(b) Effects of Thermal Aging on High-Cycle Fatigue Strength

A limited number of thermally-aged specimens have been tested to evaluate the effects of in-reactor thermal environments on fatigue strength. Materials tested include Type 304 stainless steel, Incoloy-800H, and normalized and tempered 2½Cr-1Mo steel. For the Type 304 stainless steel, aging was carried out at 1000 and 1200°F. Testing, however, was restricted to 1100°F because of the limited number of specimens available. For the other two materials, aging and testing was carried out at 1000°F. Testing was in air at a cycling rate of 40 Hz. A summary of the tests is given in Table 2.4.

Figures 2.8 and 2.9 show that the most pronounced effects of aging in Type 304 stainless steel are observed for an aging time of about 2000 h. For a longer aging time (5000 and 9000 h), the fatigue strengths show a weakening effect, the magnitude of which depends on the aging temperature. For specimens aged at 1200°F it appears that the fatigue strength may actually be lower than that measured for nominally solution-annealed material. Thus, the assumption that solution-annealed material will always yield conservative data, since it is the weakest state, may not always be valid for Type 304 stainless steel. Care must be taken to fully evaluate potential deleterious effects due to thermal aging.

The 2½Cr-1Mo steel also shows strong evidence of thermally-induced weakening. The data in Figure 2.10 show that aging at 1000°F for 9000 h will decrease the

Table 2.3

Tensile Strain Accumulation in Unfailed, High-Cycle

Mean-Stress Fatigue Samples

<u>Spec. No.</u>	<u>Test Temp. (°F)</u>	<u>Stress (ksi)</u>		<u>Accumulated Cycles</u>	<u>Tensile Strain (%) (1)</u>
		<u>Mean</u>	<u>Cyclic</u>		
<u>Type 304 Stainless Steel</u>					
MS1-83	72	5.5	27.5	7.2 x 10 ⁷	1.78
MS1-87	72	6.0	30.0	6.6 x 10 ⁶	0.64
MS1-98	1100	6.0	22.0	1.0 x 10 ⁸	13.17
MS1-89	1100	6.0	22.0	7.2 x 10 ⁷	2.61
MS1-93	1100	8.0	20.0	1.0 x 10 ⁸	10.51
MS1-111	1100	12.0	18.0	1.2 x 10 ⁸	4.09
MS1-129	1100	20.0	10.0	1.7 x 10 ⁷	13.23
<u>Incoloy-800 (0.03% C, Heat HH 7531A)</u>					
MN1-71	1200	10.0	18.0	1.0 x 10 ⁸	0
MN1-72	1200	15.0	16.0	1.1 x 10 ⁸	4.96
MN1-78	1200	15.0	20.0	9.4 x 10 ⁵	9.38
<u>Incoloy-800H (0.05% C, Heat HH 7427A)</u>					
MNF-137	1100	10.0	22.0	1.2 x 10 ⁸	2.84
MNF-133	1100	20.0	20.0	1.2 x 10 ⁸	12.53

NOTE: (1) Based on diametral measurements and a constancy-of-volume criterion.

Table 2.4a

The Effect of Thermal Aging on the High-Cycle Fatigue
of Type 304 Stainless Steel at 1100°F

Specimen ⁽¹⁾	Aging Treatment			Test Envirmt.	Alternating Stress (ksi)	Cycles to Failure (N_f) ⁽²⁾
	Temp. (°F)	Time (h)	Envirmt.			
MS1-22		None		Air	24.0	1.1×10^5
MS1-23		None		Air	23.2	7.5×10^5
MS1-31		None		Air	22.4	6.0×10^4
MS1-24		None		Air	22.0	9.3×10^6
MS1-26		None		Air	22.0	2.2×10^6
MS1-55		None		Air	22.0	2.5×10^4
MS1-32		None		Air	21.2	2.5×10^4
MS1-50		None		Air	21.2	$>4.4 \times 10^7$
MS1-51		None		Air	21.2	$>4.4 \times 10^7$
MS1-57		None		Air	21.2	6.2×10^4
MS1-34		None		Air	20.8	2.5×10^4
MS1-36		None		Air	20.4	5.0×10^4
MS1-38		None		Air	20.4	9.0×10^4
MS1-39		None		Air	20.4	6.5×10^4
MS1-35		None		Air	20.0	$>1.1 \times 10^8$
MS1-40		None		Air	20.0	3.4×10^7
MS1-33		None		Air	19.2	$>1.2 \times 10^8$
WB-4	1000	2100	Vac.	Air	26.0	2.3×10^6
WB-3	1000	2100	Vac.	Air	24.0	$>6.2 \times 10^7$
WB-5	1000	2100	Vac.	Air	24.0	$>1.4 \times 10^8$
WB-6	1000	5000	Vac.	Air	25.0	3.8×10^4
W-5	1000	5000	Vac.	Air	24.0	2.0×10^4
W-8	1000	5000	Vac.	Air	24.0	1.7×10^4
W-10	1000	5000	Vac.	Air	22.0	$>1.2 \times 10^8$
WB-8	1200	1800	Vac.	Air	26.0	1.8×10^4
WB-9	1200	1800	Vac.	Air	25.0	2.7×10^4
WB-7	1200	1800	Vac.	Air	24.0	1.0×10^6
WB-10	1200	5000	Vac.	Air	23.0	2.6×10^4
W-11	1200	5000	Vac.	Air	23.0	1.9×10^7
W-17	1200	5000	Vac.	Air	20.0	7.2×10^7
MS1-76	1000	9000	Vac.	Air	23.0	5.2×10^4
MS1-78	1000	9000	Vac.	Air	22.5	$>1.0 \times 10^8$
MS1-77	1000	9000	Vac.	Air	21.5	5.0×10^7

Notes: 1. WB specimens from standard BNL heat of steel. All others from heat 55697.
2. Cycling rate is 40 Hz.

Table 2.4b

The Effect of Thermal Aging on the High-Cycle Fatigue
of Normalized and Tempered 2½Cr-1Mo Steel at 1000°F

Specimen	Aging Treatment		Alternating Stress (ksi)	Cycles to Failure (N_f) (1)
	Temp. (°F)	Time (h)		
MCl-27	None		42.0	1.2×10^5
MCl-28	None		40.0	1.0×10^6
MCl-29	None		39.2	3.3×10^6
MCl-30	None		38.4	5.9×10^5
MCl-31	None		38.4	8.1×10^7
MCl-32	None		38.4	4.3×10^6
MCl-26	None		38.0	$>6.3 \times 10^6$
MCl-36	1000	9000	42.0	2.4×10^4
MCl-37	1000	9000	38.0	1.5×10^6
MCl-39	1000	9000	37.0	4.9×10^6
MCl-38	1000	9000	36.0	$>1.0 \times 10^8$

Note: 1. Cycling rate is 40 Hz.

Table 2.4c

The Effect of Thermal Aging on the High-Cycle Fatigue
of Incoloy-800H at 1000°F

Specimen	Aging Treatment		Alternating Stress (ksi)	Cycles to Failure (N_f) (1)
	Temp. (°F)	Time (h)		
MN1-122	None		40.0	7.0×10^3
MN1-124	None		38.0	2.7×10^4
MN1-125	None		36.0	$>1.3 \times 10^4$
MN1-126	None		35.0	7.3×10^4
MN1-129	None		34.5	2.3×10^7
MN1-130	None		34.5	$>1.7 \times 10^7$
MN1-128	None		34.0	$>2.0 \times 10^7$
MN1-127	None		33.0	$>2.0 \times 10^7$
MN1-35	1000	9000	60.0	5.8×10^3
MN1-34	1000	9000	50.0	5.5×10^7
MN1-33	1000	9000	42.0	4.6×10^8
MN1-32	1000	9000	38.0	$>1.0 \times 10^8$

Note: 1. Cycling rate is 40 Hz.

EFFECT OF THERMAL AGING ON THE HIGH-CYCLE FATIGUE OF TYPE 304 STAINLESS STEEL AT 1100°F

Figure 2.8

EFFECT OF THERMAL AGING ON THE ENDURANCE LIMIT OF SOLUTION-TREATED TYPE 304 STAINLESS STEEL AT 1100°F.

Figure 2.9

EFFECTS OF THERMAL AGING ON THE HIGH-CYCLE FATIGUE OF NORMALIZED AND TEMPERED 2 1/4 Cr - 1 Mo STEEL AT 1000°F.

Figure 2.10

high-cycle fatigue strength by about 6-8 percent. On the other hand, a similar thermal aging treatment for Incoloy-800H led to a strength increase of about 35 percent (Figure 2.11).

(c) Creep-Rupture Properties of Primary Circuit Structural Materials

The safety of an HTGR depends on the maintenance of structural integrity in the primary circuit materials throughout the life of the reactor system. In this phase of our program an elevated-temperature creep testing effort has been established to study the long-term mechanical behavior of alloys in prototypic HTGR helium environments.

Investigators at General Atomic (Mazandarany, 1975) and in Europe (Wood, 1973) have shown that the morphology of oxidation of primary circuit materials differs significantly for exposures in HTGR helium and air. In an impure helium environment, an impervious protective oxide scale is not formed and this may cause preferential oxidation and penetration along grain boundaries. This mode of attack creates notches at the surface and can have significant deleterious effects on the mechanical properties.

In our testing program, the effect of impure helium environment on creep and rupture properties of Hastelloy X, Incoloy-800H, and $2\frac{1}{2}\text{Cr-1Mo}$ steel are being studied. The objectives of the work are to quantify the effect of the environment, and to study the mode of fracture in various helium environments. The MTL has been constructed to supply a helium environment with controlled amounts of various impurities for the mechanical testing of these alloys.

Eight Applied Test Co. creep-rupture test units have been set up during this quarter and all the units are operating routinely. The units are equipped with environment chambers for testing in helium supplied from the recirculating MTL. Eight additional creep test units are on order and these will be delivered in October 1977.

Figure 2.12 shows 2 of the creep units in operation. The one shown in the foreground is operating with an environmental retort using helium supplied from the MTL. The other shows a test being performed in air.

At the present time, we are testing reference heats of Incoloy-800H (Ht. HH7427A) and Hastelloy X (Ht. 4-2809). The Incoloy-800H material was solution-annealed at 2100°F and the Hastelloy X was solution-annealed at 2150°F .

We have completed 6 tests on Incoloy-800H and 2 tests on Hastelloy X. Six tests are in progress. The results obtained are tabulated in Table 2.5. Although most of the tests have been in air, one test was run in helium supplied from the MTL. This was performed to check out the retort, loop circulating, and loop control systems.

(d) Microstructure and Fracture of HTGR Graphites

Tension and compression specimens that have been oxidized in the BNL HIL

EFFECTS OF THERMAL AGING ON THE HIGH-CYCLE FATIGUE OF INCOLOY-800H AT 1000°F.

Figure 2.11

Figure 2.12. Creep Machines Being Used for In-Helium Testing (Foreground) and In-Air Testing (Background).

Table 2.5

Creep-Rupture Properties of Incoloy 800H and Hastelloy X

Material	Specimen	Temp. (°F)	Envirmt.	Stress (psi)	Time to Rupture (h)	Elongation (%)	Reduction of Area (%)	Minimum Creep Rate (in/in/h)	Comments
Incoloy 800H	MNT-179	1200	Air	26,000	132.7	26.3	41	2.5×10^{-5}	
	MNT-182	1200	Air	25,000	648.5	28.0	53	3.4×10^{-5}	
	MNT-185	1200	Air	22,000	-	-	-	4.0×10^{-6}	Test in progress 2600 hours
	MNT-184	1200	Air	20,000	-	-	-	3.3×10^{-6}	Test in progress 2550 hours
	MNT-165	1400	Air	12,000	(144.9) ⁽¹⁾	54.0	-	1.7×10^{-3}	
	MNT-163	1400	Air	10,000	790.2	55.0	53	4.8×10^{-4}	
	MNT-164	1400	Air	8,000	-	-	-	8.2×10^{-5}	Test in progress 1400 hours
Hastelloy X	MHT-19	1400	Air	19,000	(224.0) ⁽¹⁾	71.5	-	5.0×10^{-4}	
	MHT-02	1400	Air	16,000	-	-	-	$4.0 \times 10^{-5(2)}$	Test in progress 1850 hours
	MHT-18	1400	Air	14,000	2225.8	49.5	61	8.5×10^{-5}	
	MHT-01	1400	Air	13,000	-	-	-	3.3×10^{-5}	Test in progress 2000 hours
	MHT-17	1400	Helium	19,000	(223.0) ⁽¹⁾	81.0	-	6.6×10^{-4}	

Notes: 1. Test stopped. Specimen exceeded extensometer range without fracture.

2. Used a less sensitive extensometer system than in other tests.

and tested are being 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 burnoff 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 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 being observed by scanning electron microscopy. Optical micrographs were taken with partially crossed polarized light to give the microstructure some shading representing the orientation of small regions of preferred orientation.

Specimens of PGX graphite that have been tested in tension have been observed with optical microscopy. Montages at low magnification of the entire width of the against-grain tensile specimens were presented in the last quarterly report. Documented here are micrographs at higher magnification of these same specimens.

Micrographs of the fracture edge of an unoxidized tensile specimen are shown in Figures 2.13 and 2.14. The fracture edge does not appear to follow exactly the underlying microstructure. At this point, it is necessary to distinguish between the microstructure of this graphite and what might be called its macrostructure. PGX graphite appears to consist of large grain agglomerates about 600 μm in size. These agglomerates are outlined by large pores which tend to encircle the agglomerates. Each agglomerate is comprised of smaller filler coke grains of varying sizes; grains about 100 μm in size are easily identified. Thus, the fracture mode for the unoxidized PGX graphite may be described as interagglomerate, that is, the fracture path lies between agglomerates of the macrostructure.

The fractured edge of the oxidized tensile specimens, as shown in Figures 2.15 through 2.18 appears more jagged and tends to correlate with the microstructure. The small filler grains are identifiable and in some locations, the orientation of the fracture surface seems to have been controlled by these filler grains. This type of fracturing is intra-agglomerate and intergranular.

In the interior of the oxidized tensile specimens below the fracture surface, several of the filler coke grains appear to be outlined by small pores in the binder adjacent to each grain. These grains are highly anisotropic and thus their structure is most probably fine fibrous (needle). The binder, which is more isotropic, has been selectively removed by the oxidation and thus has been weakened. This weakened binder may be the cause of fracture in these oxidized tensile specimens.

Scanning electron microscopy was employed to observe the fracture surface of the unoxidized and oxidized tensile specimens. In general, this micrography did not prove helpful. The fracture surface of this polycrystalline graphite appears intricate and irreducible. It is also very difficult to relate what is observed on the surface to the microstructure beneath the surface. The optical micrography of the radial cross sections is thus much more informative.

Figure 2.13. Optical micrograph of fracture edge of PGX graphite tensile specimen G-17, against grain, unoxidized

Figure 2.14. Optical micrograph of fracture edge of PGX graphite tensile specimen G-17, against grain, unoxidized

Figure 2.15. Optical micrograph of fracture edge of PGX graphite tensile specimen 1-3-C, against grain, 2.08 w/o burnoff

Figure 2.16. Optical micrograph of fracture edge of PGX graphite tensile specimen 1-3-C, against grain, 2.08 w/o burnoff

Figure 2.17. Optical micrograph of fracture edge of PGX graphite tensile specimen 1-4-G, against grain, 10.21 w/o burnoff

Figure 2.18. Optical micrograph of fracture edge of PGX graphite tensile specimen 1-4-C, against grain, 10.21 w/o burnoff

2.2 Materials Test Loop (L. G. Epel)

The construction of the MTL was completed during this quarter and shake-down tests were begun. The tests involved exercising the impurities injection and removal systems, the data acquisition system, and the measuring systems.

During a portion of these tests one of the creep testing machines was connected to the loop and the retort heated to 1400°F. It was found that the test machine retorts are responsible for any leakage in the system because of the method used to seal them. Tests were also conducted with a spare retort in the system, both at about 1300°F and at room temperature. Starting with pure helium it was found that after 3 days of operation at elevated temperatures no significant quantities of impurities were present except for CO₂; it reached a level of 100 ppm. At room temperature, however, after about a week of operation, the CO₂ level in the loop reached only 13 ppm (the other impurity levels were nil again).

It has been found that a heat exchanger will be unnecessary for this loop, consistent with expectations at the beginning of construction. However, it now seems that an automatic control system to maintain desired levels of impurities will be desirable. A number of such systems are being designed and will be built, operated, and evaluated during the next quarter.

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

This study of PGX graphite covers two interrelated efforts: (1) physical characterization and (2) oxidation kinetics by impurities in the He coolant stream. Currently, the first of these consists of (a) elucidating the nature of the iron impurity in PGX graphite, especially as it affects its gasification kinetics; and (b) measurement of gas permeability of PGX graphite as a function of a diffusion mechanism that may be operating in an HTGR. The second effort has currently been devoted to a laboratory scale study of the kinetics of PGX graphite oxidation by H₂O in the presence of H₂ at low burnoffs.

2.3.1 Characterization of PGX Graphite

(a) In situ Mössbauer Spectroscopy

The elucidation of the role of iron in PGX graphite oxidation has been aided by the construction of a controlled-atmosphere furnace for in situ Mössbauer spectroscopy. Heretofore, the nature of the iron species present under particular reaction conditions has been inferred only by room temperature measurements of the quenched sample. The in situ measurements allow one to determine a steady-state time-averaged concentration for each iron species that exists during the gas-iron-graphite reaction. A flow system was utilized in order to minimize secondary reactions arising from buildup of primary reaction products. The method is limited, however, to temperatures below the magnetic transition temperature ($T_c = 770^\circ\text{C}$ for bulk iron) since species identification is facilitated by the magnetic splitting of the nuclear energy levels; the species must therefore exist in a particular magnetic/chemical state for ≈ 100 nsec before reacting so

that the magnetic splitting is not time-averaged to zero. Since the magnetic transition is a function of particle size, the applicability of the method vis-a-vis the iron particle size in PGX must first be checked.

A PGX sample known to contain ≈ 1100 ppm iron was pretreated by reducing in H_2 , oxidizing in dry air and finally reducing in H_2 for 17 hours at $750^\circ C$. Mössbauer spectra were obtained in a He/8.5% H_2 environment as a function of temperature. The results are shown in Figure 2.19. The separation of the outermost peaks is a measure of the magnetization and this is shown as a function of temperature in Figure 2.20. It is noted that the iron particles in PGX behave magnetically in a manner similar to bulk iron; the particle size of the iron in PGX is, therefore, much larger than the $120\text{\AA}$ diameter minimum which had previously been determined (Growcock, 1976) from room temperature measurements, assuming that the heat treatments themselves had no effect on the particle size.

A series of high-temperature measurements was also made with the sample exposed to either H_2O or $H_2O + H_2$. At $650^\circ C$ with $\sim 3\%$ H_2O in He only the single peak due to $Fe_{1-x}O$ was observed and a gas chromatography-determined ratio of $[CO]/[H_2O] \sim 5 \times 10^{-4}$ was obtained. At $550^\circ C$ ($[CO]/[H_2O] \sim 0.9 \times 10^{-4}$) and $400^\circ C$ higher oxides dominated the spectrum although a detailed analysis is currently hampered by the large line widths which were encountered.

In the experiments with $\sim 3\%$ H_2O and varying amounts of H_2 , the temperature was fixed at $650^\circ C$ in order to achieve reasonable oxidation rates and yet obtain simple combination spectra of Fe, $Fe_{1-x}O$, and possibly iron species intermediates formed during the oxidation process. Low time-averaged concentrations of the latter precluded their being observed and, again, enhanced line widths (as yet unexplained) yielded complex spectra. Qualitatively they appeared to be a mixture of Fe + $Fe_{1-x}O$. With $\sim 10\%$ H_2 after $\frac{1}{2}$ -hour exposure the $[CO]/[H_2O]$ ratio was ~ 0.2 . This declined to ~ 0.02 after 4 hours; after 60 hours exposure and $[H_2] \sim 50\%$ the ratio had decreased further to ~ 0.002 . The consistent presence of Fe during these runs indicates that it is an active participant during the catalyzed reaction, and is apparently formed continuously by the action of H_2 . However, the dependence of the reaction rate on $[H_2]$ is obviously more complicated, since large concentrations appear to inhibit the rate.

(b) Permeability of PGX Graphite

Gasification of the core support block material, PGX graphite, is of some concern, as it may compromise the structural integrity of the graphite. Penetration of reactive impurity gases from the helium coolant stream deep into the matrix may lead to enhanced gasification of the graphite (Everett, 1967), with a concomitant loss of strength. Consequently, we have initiated a study of the flow of various gases through PGX graphite with a view toward determining the extent of gasification. In addition, the techniques are sensitive enough to provide detailed information about pore structure variations within PGX graphite.

The flow of gases through a porous material may be expressed as the sum of several independent processes, their relative contributions varying with environmental conditions. The processes expected to be significant in an HTGR environment include gaseous transport via a pressure gradient, transport via surface

Figure 2.19. Mössbauer Spectrum of 1100 ppm Fe in PGX Graphite. The iron is in a predominantly reduced metallic state.

Figure 2.20. Internal Magnetic Field of the Iron Particles in PGX Graphite as a Function of Temperature.

diffusion and gas diffusional flow. We are currently addressing the first of these: flow resulting from a pressure gradient.

Gas permeability measurements are being conducted on graphite discs for various pressure and temperature regimes. Preliminary experiments on a 1 g disc 0.54 cm thick (L) with an average diameter of 1.12 cm cut along the axis of the end piece of a PGX log have been performed over the temperature range 20°C - 800°C with helium at mean pressures (p_m) of 0.1 to 1.1 atm and flow rates (q) of 15 to 700 cm³/min. The disc was precision ground to a slight taper, which would permit its placement in the complementary precision-ground temperature-controlled region of a quartz tube (Growcock, 1977).

For viscous (Poiseuille) flow of gases through consolidated porous media the permeability, K , is defined by Darcy's law (Carman, 1956):

$$K = \frac{q p_m L}{A \Delta p} = \frac{B_o p_m}{\eta} \quad (2.7)$$

in units of cm² sec⁻¹, where A is the cross-sectional area exposed to fluid flow, Δp is the pressure differential across the sample, η is the fluid viscosity and B_o is known as the Viscous, or Poiseuille, Permeability Constant. For molecular (Knudsen) flow the semi-empirical expression may be used (Carman, 1948 and Carman, 1956):

$$K = 4/3 K_o v_m \quad (2.8)$$

where K_o is known as the Knudsen Permeability Constant and, like B_o , is dependent only on the properties of the graphite; v_m is the mean molecular velocity, given by $\sqrt{8RT/\pi M}$, where R is the gas constant, T the temperature and M the fluid molar mass. For viscous-turbulent flow another semi-empirical expression may be utilized (Carman, 1956):

$$K = p_m \left(\frac{\eta}{B_o} + b \rho q/A \right)^{-1} \quad (2.9)$$

where the coefficient b is a weak function of the flow rate and ρ is the fluid density. The flow of a gas through the graphite may be expressed as the sum of these contributions, Eq. (2.8) + (2.9), with an additional term for the diffusional flow, K_d , caused by physical adsorption:

$$K = p_m \left(\frac{\eta}{B_o} + b \rho q/A \right)^{-1} + 4/3 K_o v_m + K_d \quad (2.10)$$

Diffusional flow can probably be neglected in the case of helium, assuming non-sorption. Values of K are obtained from experimental measurements of L , A , p_m , Δp , and q at p_m ; a plot of K vs. p_m should provide some indication of the relative contributions of each flow mechanism. Such a plot is depicted in Figure 2.21 for various temperatures; correcting the flow rate, q , to the

Figure 2.21. Linear least-squares plots of permeability K against mean pressure p_m for He in PGX graphite at various temperatures, utilizing average flow rates obtained at room temperature.

appropriate temperature yields straight lines for K vs. p_m and a common extrapolated intercept, $4/3 K_0 v_m = 0.55$, except for the 657°C trace, which gives $4/3 K_0 v_m \approx 0.45$. The K_0 values thus obtained and the B_0 values extracted from the slopes are given in Table 2.6. Excluding the 657°C values, we obtain an average B_0 of $(1.87 \pm 0.10) \times 10^{-9} \text{ cm}^2$ for 2σ and a K_0 which varies as $T^{-1/2}$. The straight-line plots indicate that the flow rates under these conditions are sufficiently low that $\eta/B_0 \gg bp q/A$. Turbulent flow may be obtained at only slightly higher pressures, however, as was observed by Jenkins and Roberts, 1963. At very low pressures Eq. (2.10) is expected to fail due to the onset of molecular flow and, in general, K assumes the constant value given by Eq. (2.8).

The observed temperature dependence agrees with that given by Eq. (2.10) and follows the trends observed by Hutcheon, 1958 for a fine-pored graphite in the range $20^\circ\text{--}1000^\circ\text{C}$. Ash and Grove, 1960, however, found that, using H_2 and He with a porous ceramic, K_0 increased with temperature.

The room temperature viscous flow permeability constant, B_0 , may be compared with that obtained by the manufacturer. Union Carbide Corporation (Goldman, 1976) obtained values of 0.153–0.168 in units of darcies compared with our much lower value of 0.0018 darcies. No satisfactory explanation is available for this gross discrepancy. Jenkins and Roberts, 1963 obtained a good correlation between B_0 and K_0 for a large number of reactor-grade graphites which has the form $B_0 = 0.0032 K_0^{1.32}$. The authors interpreted their results in terms of a simple parallel pore model (Adzumi, 1937); assuming that all the gas molecules undergo diffuse reflection at the capillary walls, the graphites may be represented by a model containing 2.2×10^6 straight circular capillaries of radius $= 1.6 \times 10^{-5} \text{ cm}$. Our PGX sample possessed a Poiseuille Permeability Constant higher than that of any of the graphites examined by Jenkins and Roberts, 1963; it may, nevertheless, be correlated with K_0 by an expression similar to that above: $B_0 = 0.0305 K_0^{1.32}$. Utilizing the same model we obtain a pore structure for PGX graphite represented by 1.7×10^3 straight circular capillaries of radius $= 1.3 \times 10^{-3} \text{ cm}$.

Uncertainties in the theoretical interpretations of B_0 and K_0 preclude accurate correlation of deduced geometric parameters among different graphites. However, useful information on relative tortuosities, specific surface areas and shape factors may be obtained when permeability measurements of PGX samples from various regions of the graphite log are measured. In addition, a high- p_m , high-temperature apparatus for measuring permeabilities in the turbulent flow regime is almost completed. The large pores that PGX graphite is expected to contain should facilitate turbulent flow at relatively low p_m 's, but large samples are needed to obtain representative permeability values (Hewitt, 1960).

Experiments will also be conducted in this apparatus with reactive gases normally found in the HTGR coolant, namely H_2O , CO , and CO_2 . Sorption of these gases is expected to play an important role in their transport through PGX graphite. Toward this end, we also anticipate obtaining some BET adsorption data for these gases on PGX graphite.

Table 2.6

Permeability Constants for PGX Graphite Sample No. 5
at Various Temperatures

Temperature ($^{\circ}\text{C}$)	B_o (cm^2)	K_o (cm)
20	1.78×10^{-9}	3.31×10^{-6}
386	1.91×10^{-9}	2.21×10^{-6}
464	1.90×10^{-9}	2.09×10^{-6}
569	1.89×10^{-9}	1.95×10^{-6}
657	2.46×10^{-9}	1.60×10^{-6}
760	1.87×10^{-9}	1.76×10^{-6}

2.3.2 Oxidation Kinetics of PGX Graphite

Oxidation experiments on untreated 1-gram discs of PGX graphite with iron concentrations ranging from <40 ppm to >1500 ppm were conducted in the flow-by (thermogravimetric) and flow-through systems described in the previous quarterly progress report (Growcock, 1977).

Total burnoff of the samples was kept to <0.1%, which proved to be acceptable since the reaction rate was observed to be a relatively weak function of burnoff below this level. Total flow rates in the flow-by system were varied from 650 to 1350 cm^3/min at temperatures of 550 to 830 $^{\circ}\text{C}$; in the flow-through system flow rates of 100 to 200 cm^3/min were used at 750 $^{\circ}\text{C}$. H_2O and H_2 were added in varying amounts to He up to $[\text{H}_2\text{O}] \sim 2.0\%$ and $[\text{H}_2] \sim 8.5\%$ at ~ 1.1 atm; for some experiments $[\text{H}_2]$ and $[\text{H}_2\text{O}]$ were controlled to give $[\text{H}_2]/[\text{H}_2\text{O}] \sim 10-20$, in keeping with the expected $[\text{H}_2]/[\text{H}_2\text{O}]$ values obtained under normal HTGR operating conditions (GASSAR).

Generally, experiments were conducted in the flow-through system with samples which were first treated in the flow-by system. Initial oxidation of the samples by H_2O in the flow-by system at 750 $^{\circ}\text{C}$ yielded CO_2 and CH_4 in amounts comparable to or greater than that of CO . However, they decreased rapidly, becoming negligible regardless of the treatment accorded the sample. In the flow-through system, on the other hand, consistently large values of $[\text{CO}_2]$ and $[\text{CH}_4]$ were obtained.

From mass balance considerations, the overall molar rate of graphite gasification may be expressed by:

$$r_m = - \frac{dm}{dt} = k_m m [\text{H}_2\text{O}]^n = \frac{d[\text{CO}]}{dt} + \frac{d[\text{CO}_2]}{dt} + \frac{d[\text{CH}_4]}{dt} \quad (2.11)$$

where m is the number of moles of graphite, t is the time, k_m is the molar rate coefficient and n is the order with respect to $[H_2O]$. Thus, in a flow system (at low H_2O conversion)

$$k_m = \frac{[CO] + [CO_2] + [CH_4]}{m [H_2O]^n \tau} \quad (2.12)$$

where τ is the average residence time of the reacting molecules. For laminar flow in the flow-by system this is given by $\tau = L/(q/A)$, where L is the sample length, q is the total flow rate in the reaction zone and A is the annular cross-sectional area around the cylindrical sample (assuming the sample to be impermeable to the gas flow); thus, $\tau \sim 0.04$ – 0.08 sec, which is probably sufficiently small to preclude secondary reactions. This agrees with our observation that CO_2 and CH_4 are not obtained in the flow-by system under steady-state conditions; furthermore, apparently gas-phase reactions downstream of the sample are also negligible. Eq. (2.12) may, therefore, be simplified to give:

$$k_m \sim \frac{[CO]}{m [H_2O]^n \tau} \quad (2.12')$$

The reaction order, n , for PGX graphite oxidation by H_2O was determined in the absence of added H_2 to be $\sim 3/4$ up to $\sim 5,000$ ppmv, which agrees well with the value 0.72 obtained by Blakely et al., 1964 for other nuclear graphites. In the presence of 8.5% H_2 $n \sim 3/4$ again, but began to fall off at a level of $[H_2O]$ which depended on the sample. At H_2O concentrations as high as 1% n varied from ~ 0.4 for the sample containing <40 ppm iron to ~ 0.75 for the sample containing 1500 ppm iron. Surface area considerations dictate that $n \rightarrow 0$ at high $[H_2O]$ (Walker, 1959). Experimental rate coefficients, k_m , for both the "non-catalyzed" and "catalyzed" reactions between H_2O and PGX graphite at $750^\circ C$ are listed in Table 2.7, assuming $n \sim 1$ for the sake of simplicity. It is of interest that the trend for k_m vs. [total iron] is similar for both the non-catalyzed and catalyzed reactions, which suggests that either sufficient reduction of the iron oxide is occurring (by the H_2/CO produced in the reaction or by the carbon matrix itself) or the iron oxide is exhibiting some catalytic activity. Crude estimates of the overall Arrhenius activation energy, utilizing Eq. (2.12'), give $E_a = 45$ – 50 kcal/mole for the three samples containing the highest iron levels; no dependence on $[H_2]/[H_2O]$ was found, but the data are quite sparse.

Some of the trends observed to date may be recapitulated by referring to some typical results. The relative yields of CO obtained from oxidizing a PGX graphite sample (<40 ppm iron) under various conditions are depicted in Figure 2.22. The initial reactivity toward H_2O is quite low in the absence of added H_2 ; upon adding H_2 ($[H_2]/[H_2O] \sim 18$) the reactivity jumps 30-fold (for most samples the increase was ~ 200 -fold). All the reactions (whether H_2O , H_2O/H_2 or H_2) exhibit a time dependence, where the $[CO]/[H_2O]$ vs. time displays the

characteristic exponential approach to a steady-state. Whereas reaction with H_2O leads to a limiting value of $[CO]/[H_2O]$ after 15 min, the H_2O/H_2 and H_2 reactions require several hours before the $[CO]/[H_2O]$ levels off.

The reduction of the oxidation rate by large $[H_2O]$ in the presence of H_2 is evident in the case where $[H_2O]$ was increased from 0.86% to 2.0%. No dependence on the flow rate was observed in the flow-by system; on the other hand, in the flow-through system $[CO]$ varied inversely with the flow rate ($[CO_2]$ and $[CH_4]$ appeared to be rather insensitive to either the flow rate or $[H_2]$). Secondary reactions are certainly occurring in the flow-through system so that k_m will be given by Eq. (2.12). It is noteworthy, but not surprising, that yields of CO in the flow-through system are much greater than in the flow-by system; although the reduced flow rate is primarily responsible for this, the mass transfer process needs to be examined more closely to obtain quantitative correlations between the two systems. In addition to this effort, oxidation kinetics of PGX graphite are being reinvestigated in a more systematic study in order to generate precise kinetic parameters.

Table 2.7

Molar Rate Coefficients of PGX Graphite Oxidation by H_2O^a
in Flow-By System^b

Sample No.	[Total Iron]	$[H_2]/[H_2O] \sim 0$	$[H_2]/[H_2O] \sim 20$
1	>1500 ppm	$0.186 \text{ M}^{-1} \text{ sec}^{-1}$	n.a. ^b
2		$0.10 \text{ M}^{-1} \text{ sec}^{-1}$	$25.8 \text{ M}^{-1} \text{ sec}^{-1}$
3		$0.065 \text{ M}^{-1} \text{ sec}^{-1}$	$12.8 \text{ M}^{-1} \text{ sec}^{-1}$
4		n.a.	$2.82 \text{ M}^{-1} \text{ sec}^{-1}$
5	<40 ppm	$0.014 \text{ M}^{-1} \text{ sec}^{-1}$	$2.56 \text{ M}^{-1} \text{ sec}^{-1}$

^a $[H_2O] \sim 5000 \text{ ppmv}$ ($\sim 6.0 \times 10^{-5} \text{ M}$ at 750°C), reaction order assumed to be ~ 1.0 .

^bn.a. = not available; the conversion of these molar rate coefficients to area rate coefficients is given by $k_A = 0.0876 k_m$ in units of $\text{l-cm}^{-2}\text{-sec}^{-1}$ for $r_m = k_A A [H_2O]$.

Figure 2.22.
Oxidation of an un-treated 1.16 g PGX graphite sample (<40 ppm Fe) under transient conditions in He at 750°C.
Flow-by System: 390 cc/min. Flow-Through System: 146 cc/min at 0.0 and 0.33 min; 97 cc/min at 0.67 and 1.0 min; 187 cc/min at 1.33, 1.67 and 2.0 min.

A. ~0.86% H₂O; B. ~0.86% H₂O + 8.5% H₂; B'. ~0.86% H₂O + 8.5% H₂, 656°C; C. ~2.0% H₂O + 8.5% H₂;
D. 8.5% H₂

2.4 Cesium Iodide Interaction with Materials (S. Nicolosi)

Cesium and iodine vapor, when present in the HTGR, are likely to form cesium iodide before release to the secondary containment during a variety of accident scenarios. Since cesium iodide is extremely non-volatile relative to its constituent elements, a significant effect upon the release of cesium and iodine is expected. Of interest are the possible reactions of cesium iodide with HTGR components and environments which will enhance the release of fission products. Also of interest is the chemical state of cesium and iodine in HTGR fuels since this will affect the release and migration of these elements upon fuel failure.

To date, a number of experiments have been performed on cesium iodide systems. Some results are summarized in Table 2.8 for experiments performed in flowing systems. It is shown that cesium iodide apparently reacts with air, oxygen, and nitrogen. Since very little free iodine is observed in the reaction with air, current concern is the nature of the product from the reaction with nitrogen and whether or not the reaction with nitrogen inhibits the formation of free iodine produced by the reaction with oxygen. The colored material obtained in many of these experiments has been analyzed by powder X-ray diffraction. The strongest diffraction lines are coincident with those of CsI; however, other components are indicated. The identification of these other components is currently being pursued. Since the results of cesium iodide experiments are sensitive to the presence of oxygen and nitrogen, and since Cs_2O reacts readily with water vapor, the acquisition of a dry box was necessary. This is currently being renovated since it had been used in another program. The facility can be evacuated when the gloves are not in use and is expected to be operational in early July. Experiments are also being prepared to examine the $\text{CsI} + \text{UO}_2$ system and to further study the $\text{CsI} + \text{Cs}_2\text{O}$ system.

Table 2.8

Summary of Cesium Iodide Interaction Experiments

<u>System</u>	<u>Approximate Temp. ($^{\circ}\text{C}$)</u>	<u>Results</u>
CsI + Air	500-700	Mostly yellow, orange, and brown deposits in cold trap, with some iodine.
CsI + Air + Graphite (PGX)	850	
CsI + Cr_2O_3 (~50/50 mix)	700	No reaction.
CsI + 10% O_2 in He	620	Iodine collected in cold trap.
CsI + N_2	620	Yellow, orange, and brown deposits in cold trap.
CsI + Cs_2O (~50/50 mix) (in vacuum)	400	Color change.

2.5 Helium Impurities Loop (L. G. Epel)

At the conclusion of the dynamic leak test (Run C-1) of the Helium Impurities Loop (HIL) it was noted that CO and CO₂ were inexplicably present at concentrations of ~600 ppm and ~500 ppm, respectively, despite the fact that the graphite bed (and the purification system) were isolated from the rest of the loop during the entire test. The loop was subsequently evacuated and then recharged with fresh helium and Run C-2 was conducted. This time the CO, CO₂, CH₄, H₂ and H₂O were monitored continuously for the duration of the run (over 9 days). Figure 2.23 depicts the results of this test. If the discontinuities due to instrument range changes, helium additions, recalibrations, etc. are disregarded, the general trend is seen to be a monotonic buildup in CO, CO₂, CH₄ and H₂ to varying degrees (water vapor was nil throughout this run). The appearance of hydrogen is commonly noted in metal loops of this type but the buildup of CO, CO₂ and CH₄ to impurity levels of ~450, ~400, and ~100 ppm, respectively, was unexpected. One possible explanation offered was that the new high temperature valve that isolates the graphite bed from the rest of the loop could have been leaking slightly. This hypothesis led to Run C-3.

Before Run C-3 commenced the loop was again evacuated and fresh helium charged into the system. During Run C-3 a vacuum was maintained on the tank containing the graphite bed so that any leakage would be from the loop to the bed, rather than in the other direction. Also, during this test the heating of the bed itself was eliminated; this had no significant effect on the temperature profiles in the active part of the loop. Figure 2.24 summarizes the results of the run. The curves are somewhat smoother because fresh helium was constantly bled into the loop to make up for the ~1% per day loss due to leakage. Again, the H₂O level was found to be nil throughout the run and during the run the H₂ from the metal walls appeared to become depleted.

The CO built up at a significantly slower rate than in the previous test (about one half as fast as before) and the CO₂ built up at a slightly faster rate. The CH₄ repeated its earlier performance, reaching a little over 100 ppm in a week. Since these impurities could no longer be attributed to the graphite bed, contamination in the loop itself became suspect.

Accordingly the retorts were dismantled and a small amount of particulate matter vacuumed out of them. The Viton o-rings in the retorts were replaced by Teflon o-rings and the line leading from the pump outlet to the preheating section of the loop was wrapped with nichrome wire and insulated and heat was supplied to it while fresh helium circulated through the system. Finally, a vacuum was maintained on the entire loop, through a cold trap, for over 30 hours. Subsequent to these preparations, Run C-4 was conducted as before, with a vacuum on the graphite bed and no heat supplied to the bed. The results of this test are shown in Figure 2.25. As in the other tests, the H₂O was nil; the H₂ was down a factor of almost 20 from Run C-2 and a factor of over 10 from Run C-3. The CO was down by factors of about 10 and 5, respectively from these runs and CH₄ was down by a factor of about 2. The CO₂, however, reached higher values than ever before. It was decided that an experiment would be conducted in which a 50% oxygen-50% helium mixture would be circulated in the loop. This, it was argued, might expedite the "burning off" of any carbonaceous material residing in the loop.

Figure 2.23. Impurity buildup in HIL during Run C-2.

Figure 2.24. Impurity buildup in HIL during Run C-3.

Figure 2.25. Impurity buildup in HIL during Run C-4.

Accordingly, Run C-5 was performed. This run consisted of four repetitions of the same basic experiment. Before each run the loop was evacuated through the cold trap by means of the vacuum pump and a fresh 50% oxygen-50% helium mixture was then introduced. The four experiments (A, B, C, and D) lasted approximately 43 hours, 30 hours, 30 hours, and 29 hours, respectively, and are summarized in Figure 2.26.

In Test A there was a ~2 hour loop warmup period during which the CO₂ level, starting at zero, increased sharply to above 600 ppm. For the next 18 hours the CO₂ level increased slowly but steadily to about 900 ppm. During the next 2 hours the line from the pump discharge to the preheating section of the loop was heated and the CO₂ level responded by increasing above 1100 ppm. After the discharge pipe heat was removed (~22 hours) the CO₂ level again increased slowly, until at the end of the test it reached a concentration of about 1200 ppm. Notice the response at 43 hours due to the re-application of heat to the pump discharge line.

In Test B the loop was recharged and brought up to operating temperature again. The heating of the pump discharge line, during the fifth hour, again accelerated the rate of CO₂ buildup. The next 18 hours, during which heating of the discharge line was discontinued, resulted in only a modest rise in CO₂ concentration (from ~175 ppm to ~240 ppm). These were roughly 1/4 the levels observed during the similar period in Test A. After 23 hours, heat was applied to the discharge line for the remainder of the test. The CO₂ level rose quickly, reaching over 800 ppm by the end of the experiment (30 hours). As in Test A, the concentrations of the other impurities remained at or near zero throughout Test B.

Test C shows the effect of maintaining the pump discharge line at temperature during the entire experiment. A fresh 50% oxygen-50% helium mixture was charged at the beginning of the run and the loop brought up to operating temperature. Although the rise in CO₂ concentration was not as dramatic as when the discharge line was sporadically heated in the previous two tests, it was quite rapid, reaching a level of about 1100 ppm in 30 hours. The other impurities remained essentially nil, as before.

Test D was a repeat of Test C starting with a fresh charge of gases. The rate of rise of CO₂ in this experiment was roughly 40% that in Test C, ending up with a CO₂ concentration of about 435 ppm at the end of 29 hours.

At this point it was decided to observe the impurity buildup once more, starting with pure helium and running under normal operating conditions (i.e. no heat applied to the discharge line). Run C-6A lasted 119 hours and at the end of this time had no discernible impurities except CO₂ and O₂. The carbon dioxide level was about 208 ppm at the end of the run. Because oxygen was in evidence a reducing atmosphere of hydrogen in helium was circulated in the next test, Run C-6B. This test lasted 24 hours and reduced some of the hydrogen as evidenced by the depletion of H₂ and formation of H₂O to a level of 45 ppm. At the end of this run there was a CO₂ level of 106 ppm. This test was repeated (Run C-6C) for another 24 hours. Again water built up to a level of 41 ppm but

Figure 2.26. Impurity buildup in HIL during Run C-5.

this time the CO₂ level reached only 53 ppm. Carbon monoxide reached a concentration of 28 ppm in this test whereas in the previous one it never exceeded 7 ppm. It thus appeared that the oxygen in the system was beginning to be flushed out. Finally the test was repeated one more time (Run C-6D), again for about 24 hours. The H₂O, CO₂, and CO levels reached 11 ppm, 22 ppm, and 16 ppm, respectively, thereby indicating that the loop was, at last, reasonably free of contamination.

A 71-hour run (C-7B) was conducted to see how reproducible the result of Run C-6D was. This time the CO level reached 21 ppm and the CO₂ level reached 37 ppm, values which are reasonably consistent with those attained in C-6D. In order to remove any possible CO₂ that might be present in the supply helium, an ascarite filter was inserted into the supply line. A 96-hour test (Run C-7C) with fresh helium yielded a final CO level of 4 ppm and a final CO₂ concentration of 77 ppm. These levels are sufficiently low so that the carbon monoxide injection experiments can proceed during the next quarter.

2.6 Safety Instrumentation and Control Systems (S. Nicolosi)

2.6.1 Impurity Monitoring

The Varian gas chromatograph which has undergone modifications to sample automatically for hydrogen gas is fully operational and is currently being employed on the HIL for loop chemistry experiments. The HIL nondispersive infrared monitors for CO and CO₂ have undergone modifications to operate in the range of 0-5% and are currently undergoing calibration. This range change will permit loop chemistry experiments to be performed in higher CO and CO₂ concentration regions.

2.6.2 Monitoring of Impurities in the Primary Coolant of Fort St. Vrain

Construction of the mass spectrometer is continuing. The instrument has been tested under vacuum and has produced spectra of its residual gas. Additional features still await installation such as the motorized variable slits. The vendor is also investigating the possible use of a perfluorinated diffusion pump oil (Fomblin) to replace mercury. This will produce additional background peaks, but it should eliminate the need for a liquid nitrogen cold trap which would be a distinct advantage.

PUBLICATION

SOO, P. and CHOW, J. G. Y.; "The Effects of Mean Tensile Stresses on High-Cycle Fatigue Life and Strain Accumulation in Some Reactor Materials," Brookhaven National Laboratory, BNL-NUREG-50654, May 1977.

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3. Structural Evaluation

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

Investigations of the effects of simultaneous horizontal and vertical excitation were initiated during this period. A new test arrangement was designed such that the test rig was mounted on a mechanical, All-American, vertical vibration machine. This system has the capability of inputting sinusoidal frequencies from 5 to 55 cps at adjustable amplitudes from 0 to 0.125 inches. Since our previous tests on the vertical stack of blocks were from 5 to 20 cps, the mechanical machine covers the frequencies of interest. In the present arrangement no adjustment in phasing is possible beyond random stopping and starting.

To carry out the test involving simultaneous excitation, the test rig is driven vertically by the mechanical machine and horizontally by the MB electromagnetic machine. To accomplish this, teflon pads were secured to the surface of the mechanical machine so that the test rig could slide horizontally on it. A new driving adapter was designed to couple the test rig with the electromagnetic shaker. The driving adapter consists essentially of a double pin link with spherical bearings. The spherical bearings allow some degree of lateral and vertical relative motion between the opposite ends without imparting any loading moment to the electromagnetic machine. The link also acts as a rigid connector in the driving direction. The driving link is secured to this test rig at its center of gravity so as to minimize rotational effects. This arrangement permits the electromagnetic shaker to drive the test rig horizontally over the surface of the mechanical machine. At the same time, the mechanical machine inputs independent vertical motion. Initial tests have begun and possible interactive effects are being examined.

The equations of motion for a cluster of hexagonal blocks constrained by peripheral springs have been written. This was done to develop an analytical tool with which to study the three-dimensional effects. These equations express the motions and forces that develop in a vertical cluster of hexagonal blocks which are subjected to either horizontal or vertical input motions. Compilation of a new code incorporating these equations has been initiated.

In this system, a single central block is surrounded laterally by six other blocks with a clearance distance surrounding each block. The cluster of seven blocks is surrounded by springs that project from the containment wall. A similar array of seven blocks is both above and below the central group. The intention is to study the entire array for a vertical stack with up to 10 blocks in each column.

Each block in the array is assigned 5 degrees of freedom. The blocks are free to move along the three coordinate axes and rotate about two of them. Only the motion about the vertical axis is constrained.

A conceptual design of a three-dimensional test rig has also been completed. The rig is intended to test a total of 70 hexagonal blocks. The array will be 10 blocks high with 7 blocks in a horizontal cluster. This is the same arrangement for which the equations of motion have been written. The test is intended

to study visually and photographically the motion that actually results from a particular input. Possible interactive effects can be observed. In addition, the test is also intended to give guidance for carrying out computer runs under various conditions of interest.

Computer code runs with OSCVERT have been carried out with three columns, each column with up to 9 elements. For these runs, horizontal input excitations at forcing frequencies of 6 and 8 cycles/sec. have been applied. These are the same frequencies that have been used previously with the single vertical column and that have been carried out under actual test.

3.2 Steam Generation Tube Weld Evaluation (M. Reich, M. Subudhi, T. Tanaka, P. Bezler)

The investigation of the structural integrity of the HTGR steam generator tubes continues. The progress during this period includes:

- (1) Completion of numerous elastic analyses in Phase I;
- (2) Completion of proof studies on the LEFM Code;
- (3) Initiation of some analyses of weldments as per the information supplied by GA;
- (4) Initiation of a debug routine of the FEPLLOT, a finite element code designed to plot stress and temperature contour plots; and
- (5) An initiation of inelastic analyses for the more severe loading conditions.

3.2.1 Elastic Analyses (GASSAR Data)

It has been noted that materials with similar chemical compositions can have significant differences in their mechanical and physical properties. Therefore, when two tubes, of different materials or the same material, are joined together with a different filler material and subsequently subjected to high pressure and/or thermal loadings, it is possible that local high stresses may be created due to the difference in the physical properties of the base metals, heat-affected zone (HAZ) and the weld metal. These localized stresses may eventually cause catastrophic failure necessitating difficult field repairs and possible nuclear contamination of the secondary fluid. Investigations by Wei (1976) and Dalcher (1976) have been carried out for large diameter bimetallic welded joints in conjunction with the LMFBR program. The purpose of this study is to establish the stress conditions at the weldments of small tubes when subjected to the loading conditions corresponding to the service life of a HTGR plant.

Figure 3.1 shows the finite element grid of a typical weldment. The same model is used for both the thermal code and the stress code. Three different weldments are considered in order to envelope all the possible existing welds in a steam generator. The first case is a I-800 to I-800 weld with I-82 used as the weld metal representing a typical weldment in the superheater bundle.

FIGURE 3.1

FINITE ELEMENT MODEL FOR A HTGR STEAM GENERATOR TUBE WELDMENT

The second case is similar to the first, but with different dimensions and loading conditions corresponding to its location in the reheater bundle. For both cases only the left half of the model, shown in Figure 3.1, is used because of symmetry. The third case involves two different metals with I-800 and 2 1/4 Cr-1 Mo steel as parent materials and I-82 as the filler metal. It represents a typical bimetallic weld in the main steam bundle. The outside diameter and thickness for each case are predicted from the information given in GASSAR and are summarized in Table 3.1. The fluid temperatures, the mass flow rate, and other pertinent data were taken from GASSAR and LTR. Reasonable assumptions were made to obtain moderately severe conditions for each case under consideration. Table 3.1 shows the primary and secondary fluid conditions assumed in the analyses. The temperature distributions in the tube walls during normal full load operating conditions were obtained by considering the appropriate heat transfer coefficients between helium, tubes, and water, with thermal conductivities corresponding to the tube materials being used.

TABLE 3.1

Finite Element Model Data

Case	Outside Diameter (inch)	Thickness (inch)	Fluid Temperature (°F)		Pressure (PSI)	
			Water/Steam	Helium	Internal	External
I	1.125	0.20	850	1050	2600	710
II	1.625	1.14	820	1240	640	725
III	1.125	0.20	800	1000	2600	710

The temperature distributions obtained from the thermal analysis were linearized and input into the finite element stress code. Internal and external pressures were also applied simultaneously. Material properties given by Soo (1977) were taken at the average midwall temperature. The HAZ was assumed to have the same material properties as the parent material. It should be noted that only elastic material properties are considered. A reference temperature of 1350°F is chosen corresponding to the post weld heat treatment. At this temperature a stress-free state is assumed to exist.

Stress distributions for each case under consideration have been summarized in Figures 3.2, 3.3, and 3.4. These plots include the variation of radial, longitudinal, and circumferential (hoop) stress components from the inside to outside surface for different locations of the weldment. The shear stresses were found to be very small in magnitude and hence are not shown in these figures. The effective stresses near the fusion zone are also shown. It should be noted that all the above stresses are the sum of the stresses produced by three loading conditions: the internal and external pressures, thermal gradient across the tube wall, and the total temperature change from the stress-free state.

FIGURE 3.2

STRESS DISTRIBUTIONS WITHIN THE TUBE WALL THICKNESS OF A
I - 800 WELDMENT IN THE SUPERHEATER BUNDLE FOR NORMAL
OPERATING CONDITIONS

S_1 = RADIAL

S_2 = LONGITUDINAL

S_3 = HOOP (CIRCUMFR.)
 S_e = EFFECTIVE

FIGURE 3.3

STRESS DISTRIBUTIONS WITHIN TUBE WALL OF A I-800 WELD-
MENT IN THE REHEATER BUNDLE FOR NORMAL OPERATING CON-
DITIONS

S_1 = RADIAL

S_2 = LONGITUDINAL

S_3 = HOOP

S_e = EFFECTIVE

FIGURE 3.4

STRESS DISTRIBUTIONS WITHIN TUBE WALL OF A BIMETALLIC WELDMENT IN THE SUPERHEATER BUNDLE FOR NORMAL OPERATING CONDITIONS

S_1 = RADIAL

S_2 = LONGITUDINAL

S_3 = HOOP

S_e = EFFECTIVE

The stress distributions along the inner and outer wall's surfaces are summarized in Figure 3.5 for all three cases. The jump in the stress components can be distinctly observed at the respective fusion lines. The effective stresses reach their peak values on the fusion line.

The assumption of a stress-free state at the post weld heat treatment temperature of 1350°F reflects a compromise between modeling residual stresses accurately and neglecting them entirely. Studies have shown that at this temperature only 40 - 50% of the residual stresses have been relieved. However, the evaluation of the true stress state at this temperature requires the performance of a time dependent, inelastic analysis far beyond the scope of this phase of the work. To neglect residual stresses entirely, on the other hand, represents a complete departure from the real weld. In particular, for the intended linear elastic fracture mechanics studies, some account of the residual stress fields must be made. The present assumption provides a qualitative measure of the severity of conditions at the fusion line while remaining within the elastic scope of this initial study phase.

One of the major findings from this study is that the bimetallic weld between I-800 and 2 1/4 Cr - 1 Mo experiences a more severe stress condition at the I-800 - I-82 fusion line than the similar unimetallic weld (I-800 and I-800 as base metals). The longitudinal and circumferential stresses exhibit a significant increase in their magnitudes. This might be due to the effects of the additional constraints imposed by the other base metal (2 1/4 Cr - 1 Mo) with its low coefficients of thermal expansion.

The most severely stressed location is found to be the fusion line between I-800 and I-82. The stresses on inner and outer surfaces exceed yield. The inner surface experiences a tensile loading whereas the outer face goes compressive. It may be concluded that the inner face is most susceptible to possible micro-crack growth.

Larger weld groove angles have been shown to cause more serious stress conditions because of the material compatibility at the fusion zone. If time permits, a further investigation of this effect will be undertaken.

It should be pointed out that some approximations have been made in this study. Primarily these are in the physical properties of the HAZ and the decarbonized zone. For the present this is justified by the lack of material property data, the minimal extent of these material zones, and the survey character of this study.

3.2.2 LEFM Code Studies

Crack effect studies have been undertaken to define the criticality of stress conditions at the fusion zone. The capability of the LEFM Code has been increased to handle larger problems having elements and nodes up to 200 and 1500 respectively. The validity of the code has been tested against the published benchmarks by Heliot (1977) and Bowie (1965). The results are found to be within the allowable tolerance from the reference values.

During testing it has been noticed that the singular elements at the crack tip can not be used for thermal effect studies. The enriched Quad-12 elements should be used instead. For any mechanical loading, both elements work perfectly.

INSIDE WALL

I-800 WELD (SUPER)

I-800 WELD (REHEAT)

BIMETALLIC WELD

OUTSIDE WALL

FIGURE 3.5

STRESS DISTRIBUTIONS ALONG INSIDE AND OUTSIDE WALL OF
WELDMENTS FOR NORMAL OPERATING CONDITIONS

S_1 = RADIAL

S_2 = LONGITUDINAL

S_3 = HOOP

S_e = EFFECTIVE

Finite cracks have been introduced in the highly stressed regions of HTGR steam generator tube welds as discussed in the previous section. Because of the axial symmetry in the model, only circumferential cracks were introduced for the present study. The stress intensities obtained at the crack tips are found to be very small. It can be inferred that for the normal conditions of loading there is not enough energy at the crack tips to promote propagation.

The results obtained to date are preliminary. A complete report illustrating the detailed analyses and the results will be published shortly.

3.2.3 Elastic Analyses (GA Data)

As per our request, GA has supplied us with some information on the weld geometry and steady state loading conditions for different weldments. It has been noticed that the geometry of welds given in the GASSAR departs significantly from the present design practice, especially in the reheater section. The loading conditions are found to be more or less the same as predicted in section 3.2.1.

Three separate models have been formulated with the new loading conditions. A complete report of these results will be summarized in the near future. At this time, it can be concluded that the severity in stress conditions at the fusion zone remain almost unchanged.

Although GA has mentioned that the normal transients are not severe enough to be considered for design conditions, some studies corresponding to the weekly power fluctuations will be considered in future studies. In addition, an attempt will be made to obtain data corresponding to the St. Vrain nuclear reactor and then an assessment of the weldments in that steam generator design will be undertaken.

3.2.4 Temperature and Stress Contour Plots

FEPLLOT is a plotting routine which plots data from a finite element code in two- and three-dimensional plots. This code is now operational on the BNL computer facility. Existing thermal and stress codes are being modified in accordance with the requirements of FEPLLOT so that the final results could be presented as contour plots. Modification of the thermal codes are complete.

3.2.5 Time-independent Inelastic Analysis

The analyses performed in section 3.2.1 have indicated that the parent material, I-800, at the fusion line is stressed beyond the elastic stress limit. An inelastic analysis is necessary to evaluate the final condition of this weldment. Preparations to perform these analyses with the available inelastic stress codes is underway.

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4. Analytical

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

The computer program, SODEMME, which solves transient thermal-hydraulic problems during natural circulation has been extended by adding an option in subroutine HTRNSFR. This additional feature allows the code to compute heat transfer in long channels, such as the coolant holes in an HTGR core, while the surface temperature of the channels varies arbitrarily with time. The other options available in the program have been simplified and streamlined to make the code faster during execution.

A sample problem has been formulated and used to test the program. The 3000 MWth HTGR was modeled with 9 volumes/segments and temperature ramps for the upper and lower reflector surfaces and the core graphite was assumed. The problem was run on the BNL CDC 7600 system to simulate over a half-hour of reactor history. The computing cost, using normal priority, was about 2¢ for each reactor-second.

The user's manual, which describes the theory and use of the code and contains the sample problem in detail, has been written and will be published as a topical report during the next quarter.

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

The computer code CHAP (Composite HTGR Analysis Program (Gilbert, 1977) has now been converted and is available on the BNL CDC 7600 computer. Work is continuing in the areas of obtaining a better understanding of each of the code modules and improving the efficiency of certain sections of the code which currently use a large proportion of the computer running time.

The computer code BAAL (Pracht, 1976) was received from Los Alamos Scientific Laboratory Library and has been converted for use on the BNL CDC 7600 computer. Output plot capability (originally coded for LASL's SC4020 Plotter) has been restored using the SC4020 Emulation Package first written for the KACHINA code. Though the sample problem included with the code has been run successfully, much work remains in understanding and running the BAAL code, due to the extreme simplicity of the sample problem and the lack of adequate documentation.

Work continues on adaptation of the KACHINA code to HTGR problems.

The codes LARC-1 (Carruthers, 1976), LARC-2, LEAF (Lee, 1976), SUVIUS, FYSMOD (Merson, 1976) and NONSAP-C have been received from the Los Alamos Library, as has the code QUIL (Lunsford, 1977).

Table 4.1 summarizes the status of the 60 codes now being developed or maintained in the HTGR Safety Code Library.

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.

L = Recently received from Los Alamos Scientific 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.
SORSG	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.
DIFFT1	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	OP	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.
BAAL	OP	NP	Calculates three-dimensional fluid flows at all speeds with an Eulerian-Lagrangian computing mesh.
LEAF	L	NP	Calculates fission product release from a reactor containment building.
SUVIUS	L	NP	Solves the behavior of fission gases in the primary coolant of a gas-cooled reactor.

TABLE 4.1 Cont'd.

<u>Program</u>	<u>Status</u>	<u>Proprietary</u>	<u>Function</u>
FYSMOD	L	NP	Calculates the two-dimensional solution of HTGR core blocks subjected to external motion.
NONSAP-C	L	NP	Calculates static and dynamic response of three-dimensional reinforced concrete structures, in addition to creep behavior.
LARC-1	L	NP	Calculates fission product release from BISO and TRISO fuel particles of an HTGR during the LOFC accident for single isotopes.
LARC-2	L	NP	Similar to LARC-1; in addition, handles release from isotope chains.
QUIL	L	NP	Solves complex equilibrium distributions in chemical environments.

4.3 Evaporation of the Non-Volatile Fission Products in the Core of an HTGR Following a Loss-of-Coolant Accident (J. M. Dickey)

There are several barriers to the release of the non-volatile fission products in an HTGR and therefore fission products will be retained within the core of the reactor for a considerable time following a loss-of-coolant accident. The first barrier is the refractory coating on the fuel particles; this retains efficiently the fission products under normal operating conditions. However, following a loss of forced circulation the core heats up and after about 9 hours half of the core has reached a temperature at which the fuel particle coatings have failed allowing the fission products to diffuse into the carbon binder. The non-volatile fission products will not be released immediately as they must first diffuse through the graphite moderator before reaching the coolant channel surfaces from which they can evaporate. In the absence of forced circulation, the flow of the coolant will be due to natural convection, and so very slow flow rates are anticipated (Schwartz, 1974). Under these conditions the gas in the coolant channels is almost stagnant and locally the impurities in the coolant will be in quasi-equilibrium with the impurities dissolved in the graphite. Fission product atoms will be carried in the coolant stream from the hotter to the cooler parts of the core and will tend to redeposit on the cooler surfaces. As the upper portion of the core heats up the fission products will eventually be carried out of the core. SORSG (Schwartz, 1974) is the General Atomic code constructed to calculate the release rate into the primary circuit. For comparison with SORSG a simple code EVAP is being written which emphasizes the evaporation stage of the problem, and which can be used for other situations in which evaporation is important.

As the helium flows through the coolant channel impurity atoms will evaporate from or deposit back onto the graphite depending on whether the partial pressure of the impurity in the coolant is lower or higher than the vapor pressure of the impurity at the local graphite temperature. As atoms evaporate from the surface of the coolant channel other atoms will diffuse from the interior of the graphite to the surface and conversely after atoms have been deposited on the surface they will diffuse away into the interior. A simultaneous treatment of diffusion and evaporation would be very complex so in the first version, EVAPI, the simple assumption is made that diffusion is sufficiently rapid compared to evaporation, that the concentration of the impurity is approximately uniform in the local graphite segment. So the surface concentration is equal to the total amount of the impurity in that segment divided by the graphite volume. It was pointed out in our previous quarterly report (Dickey, 1977) that this assumption is not always conservative, as the surface concentration may be under or over estimated depending on the direction of diffusion.

The model for the core is the same as that used in SORS and CORCON (Schwartztrauber, 1974). In the code EVAP the core and top reflector blocks are divided into a number of segments and the temperature of the graphite is taken to be uniform in each segment. The radial variation of temperature across the core is simulated by including several coolant channels which have different temperature profiles. The temperature history of the core after a loss-of-forced-circulation accident is taken from the General Atomic code CORCON.

In the analysis of mass transfer an important dimensionless parameter is what may be termed a Stagnation number, S_n ,

$$S_n = L/d \text{ Re } S_c \quad (4.1)$$

where

L = length of a segment
 d = diameter of coolant channel
 Re = Reynolds Number
 S_c = Schmitt Number.

S_n is related to the Graetz Number which occurs in heat transfer. Rearranging the terms in S_n we may write:

$$S_n = (L/v)/(d^2/D) \quad (4.2)$$

where V is the coolant velocity and D is the diffusion coefficient of the impurity in He. Thus, large values of the stagnation number correspond to flow regimes in which the time the coolant takes to traverse the length of the segment is much longer than the time an impurity would take to diffuse from the surface of the channel to the center. So physically we would expect a state of quasi-equilibrium to be attained not far from the entrance of the segment, and that at the exit of the segment that the partial pressure of the impurity would equal the vapor pressure over the surface. This asymptotic limit is found experimentally for $S_n \sim 0.07$ (Sherwood, 1953). For the flow rates anticipated in a loss-of-forced-circulation accident, a typical value for S_n is 1000. So in code EVAP this limit is used.

In EVAPI the coupled differential equation for the amount of impurity in each segment of a coolant channel can be solved using either a Runge-Kutta technique or a variable step Hamming predictor-corrector technique. Some preliminary results are shown in Figure 4.1 for a typical fission product, Europium. A coolant channel is represented by 8 segments and the top reflector is divided into 2 equal segments. The temperature history selected represents the inner part of the core and was calculated in CORCON. After about 20 hours most of the Eu has been transferred to the top reflector and after about 35 hours about half has been carried into the upper plenum.

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

The experimental program is intended to verify both underlying assumptions and the quantitative predictions of the computer codes HAZARD and CIRC that have been formulated to predict the extent to which gases will layer in the containment building (CB) of the HTGR following an accidental depressurization. At this time, only a two-gas system is being considered; i.e. one gas that issues from the PCRV and a second gas that fills the CB initially. Of course, the ultimate concern in this layering analysis is the possibility of combustion of CO and H which are non-mechanistically assumed to be initially mixed with the PCRV gas. The assumption is that these gases will simply be entrained with the PCRV gas during the early phases of depressurization (about the first 100 seconds or so). The distribution of trace

Figure 4.1. Transport of europium along a coolant channel in the center of the core. The core is divided into 8 equal segments 1-8, and the top reflector into 2 equal segments R1, R2.

gas in the CB is expected to be exactly the same as the distribution of the primary PCRV gas, at least in the short term before final settling of the gases in the CB. Thus, by analyzing the extent of mixing or layering in a two-gas system, one can assess the hazards that may result when the PCRV gas contains small quantities of combustibles.

The possibility of a small-scale test experiment was studied. By selecting the independent physical quantities that are thought to govern the HTGR depressurization and using dimensional analysis, modeling parameters and scaling laws for this problem have been derived. A series of experiments intended to explore the influence of the various parameters on layering are being considered.

Apart from the requirements that the model and full-scale system be geometrically similar, dynamic similarity of an HTGR depressurization requires that the following dimensionless quantities (identified as π_1 to π_5) have the same values in the model and the full scale system.

$$\pi_1 = \frac{P_c}{P_o} \quad (4.3)$$

$$\pi_2 = \frac{\rho_c}{\rho_o} \quad (4.4)$$

$$\pi_3 = \gamma_1 \quad (4.5)$$

$$\pi_4 = \gamma_2 \quad (4.6)$$

$$\pi_5 = \frac{\rho_o g D}{P_o} \quad (4.7)$$

where

- P = initial gas pressure in the CB
- P_c = initial pressure of gas in the PCRV
- P_o = initial pressure of gas in the PCRV
- ρ_c = initial density of the CB gas
- ρ_o = initial density of the PCRV gas
- γ_1 = ratio of specific heats of the CB gas
- γ_2 = ratio of specific heats of the PCRV gas
- g = acceleration due to gravity
- D = a characteristic system dimension (we have selected the CB diameter).

The quantities π_1 to π_5 are the modeling parameters -- a set of independent dimensionless variables which, once specified, uniquely determine the dynamics of the HTGR depressurization. From a parametric study utilizing the codes

HAZARD and CIRC, nine dimensionless parameters that are thought to be sufficient to completely specify the problem have been identified, and then reduced to the five dimensionless modeling parameters above, plus a dimensionless time.

A test system, geometrically similar in its bulk features to a full-scale HTGR containment building, will be used. The model CB diameter will be 2 feet, giving a scale of about 1:60 in linear dimensions. The top of the PCRV will consist of an interchangeable plate to allow testing of different orifice sizes. A small heater and thermocouple in the PCRV will allow changes in the PCRV gas temperatures. A fast-acting valve will initiate depressurization. Eventual modification of the apparatus for testing off-center and side leaks from the PCRV will also be possible. For preliminary tests a plexiglas CB will be used to allow recording of the depressurization and layering using high-speed photography and a dye gas in the PCRV.

Table 4.2 shows the parameters which are proposed for the small-scale experiments. In the first test series, only the modeling parameters π_1 to π_4 will be correctly scaled; that is to say, only these four groups will be constrained to the range expected in the full-scale HTGR's. By allowing π_5 to be mis-scaled, the effect of buoyancy in the CB will not be correctly simulated and insofar as buoyancy may affect layering, the layering process itself will be inadequately modeled. These tests will, however, be useful in assessing a number of the features of the scaling laws. Specifically, a systematic investigation of PCRV emptying and jet entrainment will be possible at this stage.

In the second phase of the experiments the modeling parameters π_2 and π_5 will be varied to investigate the importance of buoyancy forces in establishing or maintaining layering in the CB.

4.5 Experimental Determination of the Damping Ratio, ξ , of the Thermal Insulation Cover Plate for the PCRV (H. Fenech, I. Ganz - University of California, Santa Barbara)

The fatigue failure of the PCRV thermal insulation cover plate is strongly dependent on the energy dissipation rate of the flow induced noise by the excited cover plate. The damping ratio, ξ , is a physical parameter, in the general equation of motion of structure, controlling this energy dissipation rate. Earlier analyses have shown that in the expected range of the damping ratio of 0.1 to 0.01, the fatigue lifetime of the cover plate, subjected to turbulent flow noise, would be reduced by a factor of 20 for a reduction by a factor of 10 of the damping ratio. Because of the impossibility of estimating the damping ratio accurately in a complex structure like the thermal insulation cover plate, several measurements of that quantity were made for:

1. A full-scale carbon steel cover plate 42" x 42" x 1/4";
2. A smaller 21" x 21" x 1/16" carbon steel plate (mounted in the test section of the flow noise facility (Fenech, 1976a) at UCSB).

TABLE 4.2

	Test No. (1,2)	PCRV Gas Temperature	CV Gas (T=540°R)	P _o psia	P _c psia	Π ₁	Π ₂	Π ₃	Π ₄	Π ₅ × 10 ⁴	$\frac{R_e}{\sqrt{\rho_o P_o}} D^{(4)}$
S	1.1	He	Air	50	1	0.02	0.29	1.4	1.66	0.049	6 × 10 ⁶ ₇
E	1.2	T=1060°R		100	2	.	.	.	.	.	1 × 10 ⁷
R	.			.	.	.	.	.	.	.	.
I	.			.	.	.	.	.	.	.	.
E	1.15			750	15	.	.	.	.	.	1 × 10 ⁸ ₇
S	1.16	He	Air	50	1	0.02	0.15	1.4	1.66	0.096	1 × 10 ⁷
	1.17	T=540°R		100	2	.	.	.	.	.	3 × 10 ⁷
#	.			.	.	.	.	.	.	.	.
1	.			.	.	.	.	.	.	.	.
	1.30			750	15	.	.	.	.	.	2 × 10 ⁸ ₇
	1.31	Ar	Air	50	1	0.02	0.015	1.4	1.66	0.96	4 × 10 ⁷
	1.32	T=540°R		100	2	.	.	.	.	.	7 × 10 ⁷
	.			.	.	.	.	.	.	.	.
	.			.	.	.	.	.	.	.	.
	1.45			750	15	.	.	.	.	.	6 × 10 ⁸
S	2.1 ⁽³⁾	He	Air	50	2	0.04	0.29	1.4	1.66	0.096	1 × 10 ⁷
E	2.2	T=540°R		100	4	.	.	.	.	.	3 × 10 ⁷
R	.			.	.	.	.	.	.	.	.
I	.			.	.	.	.	.	.	.	.
E	2.15			750	30	.	.	.	.	.	2 × 10 ⁸ ₇
S	2.16	He	SF ₆	50	1	0.02	0.73	1.33(?)	1.66	0.096	1 × 10 ⁷
	2.17	T=540°R		100	2	.	.	.	.	.	3 × 10 ⁷
#	.			.	.	.	.	.	.	.	.
2	.			.	.	.	.	.	.	.	.
	2.30			750	15	.	.	.	.	.	2 × 10 ⁸ ₇
	2.31	Ar	SF ₆	50	1	0.02	0.073	1.33(?)	1.66	0.96	4 × 10 ⁷
	2.32	T=540°R		100	2	.	.	.	.	.	7 × 10 ⁷
	.			.	.	.	.	.	.	.	.
	.			.	.	.	.	.	.	.	.
	2.45			750	15	.	.	.	.	.	6 × 10 ⁸

TABLE 4.2 Cont'd

2.46	Ar	SF ₆	50	2	0.04	0.15	1.33(?)	1.66	0.96	4x10 ⁷
2.47	T=540°R		100	4	.	.	.	.	.	7x10 ⁷
.			.	.	.	.	.	.	.	.
.			.	.	.	.	.	.	.	.
2.50			750	30	.	.	.	.	.	6x10 ⁸
Full Scale	He	Air	730	14.6	0.02	0.29	1.4	1.66	3.0	6x10 ⁸
Conditions	T=1064°R	T=540°R							(Based on D=125 ft)	

- NOTES:
1. All tests to be conducted with orifices of several different sizes in the PCR.V.
 2. Selected tests will be repeated with varying initial PCR.V gas temperatures to assess importance of conduction heat transfer.
 3. Test 2.31 to 2.60 may be repeated with other, higher molecular weight gases in the CV.
 4. The Reynolds number is a reference value, based on the FCR.V gas alone.

4.5.1 Set Up for a Full-Scale Cover Plate

The set up included a 42" x 42" x 1/4" carbon steel plate fastened at its middle edges and center by means of 3/4-inch bolts and nuts (with washers having the same area of contact with the plate as the four peripheral and one central attachment fixtures in the present design). The bolts were anchored to a 2-inch thick wooden board, leaving a 3-inch gap between the plate and the board. A blanket of kaowool ceramic fiber insulation was compressed to 70% its initial thickness in this gap (same as in class A thermal barrier). The wooden board was solidly clamped to a rigid angle iron frame, see Figure 4.2. A 1/8-inch steel cable was attached to the plate at its center; this cable was hooked to the frame from the top. Tension was produced in the cable by means of a tension fixture (a turnbuckle, usually used by sailors and builders). A latch to release the tension at once when activated (in order to produce an impulse) was added to the steel cable. The set up is shown on Figure 4.2.

Since only relative amplitudes of the free vibrations are needed to determine the damping ratio and since the strain on the surface of the plate is proportional to the lateral displacement of the middle surface at the same location (Vierck, 1967), strain gages were used to measure the decaying harmonic motion that followed the release of the stressed plate.

4.5.2 Set Up for the Smaller Plate

This set up included a 21" x 21" x 1/16" AISI-1020 steel plate fastened at its middle edges with four bolts, nuts, and washers for the case of a broken or missing central attachment fixture. The bolts were anchored to a 1/4-inch rigid frame, see Figure 4.3. (For design details, see Fenech, 1976a, which includes the design and instrumentation of the test section of the flow noise facility.)

Free damped lateral vibrations were induced in the plate by hitting it and measuring the decaying motion with strain gages and LVDT's.

The resulting damping ratio for this set up is used to analyze the response of the same plate to flow noise excitation inside the test section (Fenech, 1976b). This set up is shown on Figures 4.3 and 4.6.

4.5.3 Instrumentation

Three micromasurement strain gages (EA-06-250-BG-120 Ω) were bonded to the full-scale plate and six to the smaller one at locations and directions selected to avoid nodal lines of the first few modes of vibration. Each of these strain gages consists of a constantan foil protected by a tough, flexible, prolimide backing suitable for a temperature range of -100°F to +350°F. The strain range is $\pm 5\%$. The gages are self temperature compensated for carbon steel which has a thermal expansion coefficient of 6 ppm/°F. They are 1/4-inch long and have a resistance of 120 Ω . The gage factor is 2.1.

Each of these strain gages being the active arm of a four-arm bridge was excited by a constant voltage source (7 volts D.C.) and its resistance change due to strain was converted to a low-voltage signal proportional to the strain

Figure 4.2. Experimental set up for the measurement of the damping ratio of a 42" x 42" x 1/4" cover plate.

Figure 4.3. Experimental set up for the measurement of the damping ratio of a 21" x 21" x 1/16" cover plate mounted in the flow test facility.

$\xi = 21\%$

$\xi = 12\%$

$\xi = 4\%$

$\xi = 1.8\%$

$\xi = 1.4\%$

Figure 4.4. Typical photographic traces of the vibratory damping of the cover plate.

Figure 4.5. Position of the Strain Gages on the 42" x 42" x 1/4" cover plate.

Figure 4.6. Position of the Strain Gages and LVDT on the 21" x 21" x 1/16" cover plate.

in the plate. This signal was then amplified to the millivolts level by a Tektronix AM-502 differential amplifier with a TM-506 power supply module. The AM-502 has a 1 to 100,000 gain, 100 dB common mode rejection ratio, high and low pass filters, D.C. to 1 MHz bandwidth.

The output of the AM-502 was fed into a Tektronix-564 oscilloscope with a 3B3 time base and a 2A63 differential amplifier for the display of the strain signal versus time.

Five Linear Variable Differential Transformers were mounted in the test section of the flow noise facility to measure the lateral displacement of the smaller plate. The LVDT's were Trans-Tek, Inc. Model 271-000, which has the following characteristics:

Range:	+0.01 inches
Sensitivity:	0.0357 full-scale/volt
Linearity:	+0.25% full-scale
Primary Impedance:	232 Ω
Secondary Impedance:	270 Ω

Each of the LVDT's were coupled to a signal conditioner that includes a sine wave oscillator, a 15 volts D.C. power supply and a demodulator amplifier. The combination of the LVDT's and their signal conditioner had a sensitivity of 240 volts/inch. The output of this signal conditioner was fed into the oscilloscope mentioned above for the display of the time-dependent displacement signal.

For the analysis of the signals, the strain and displacement outputs from the signal conditioners had to be frequency band passed by means of an electronic filter. An ITHAC Model 4212 electronic filter was inserted between the signal conditioners and the oscilloscope. This filter has the following characteristics: high pass 3 dB cut-off frequency range of 0.1 Hz to 10 kHz in 1/10 decade steps; low pass 3 dB cut-off frequency range of 0.1 Hz to 100 kHz in 1/10 decade steps; cut-off frequency accuracy of $\pm 1\%$; maximum attenuation of 90 dB; attenuation slope of 24 dB/octave, 80 dB/decade.

Care was taken to match the impedance of the components in the instrumentation system. The noise was reduced appreciably by using shielded cables and B.N.C. connectors, isolating the components to avoid mutual induction of voltages, and maximizing excitation transducers for high sensitivity.

4.5.4 Results

Polaroid photographs were used to record displacement and strain signals from the oscilloscope's display. See Figure 4.4 for typical traces. These prints were analyzed by means of the following relations:

- $\xi = \frac{1}{2\pi n} \ln \left(\frac{A_0}{A_n} \right)$ where A_n is the amplitude n cycles after the amplitude A_0 .
- A linear regression to obtain the best fit line for $\ln \left(\frac{A_0}{A_n} \right)$ versus $2\pi n$.

For the damping ratio measurement, examination of the effects of different accidental conditions on the damping ratio were desired. The following cases were considered:

1. A missing or broken central attachment fixture.
2. A missing or broken peripheral attachment fixture.
3. Loss of resiliency of the kaowool insulation.
4. Insulation wetted by steam leakage or water leaks into the primary system.

In addition, the effect of the fastening torque on bolts on the damping ratio has been investigated.

The results of this investigation for the different cases are listed below:

Plate Size: 42" x 42" x 1/4"

Bolts: 4 mid-edge, no washers, no center bolt (torque on bolts in ft·lb)

Insulation: None

<u>Strain Gage No.</u>	<u>Torque on Bolts</u>	<u>Damping Ratio x 10² (%)</u>
1	0	.137
2	0	.171
3	0	.182
1	50	.085
2	50	.103
3	50	.097
1	150	.078
2	150	.096
3	150	.126

Data is for the 38 Hz mode. See Figure 4.5 for position of Strain Gage.

Plate Size: 42" x 42" x 1/4"

Bolts: 4 mid-edge with washers, no center bolt (torque on bolts in ft·lb)

Insulation: None

<u>Strain Gage No.</u>	<u>Torque on Bolts</u>	<u>Damping Ratio x 10² (%)</u>
1	0	.166
2	0	.169
3	0	.223
1	50	.110
2	50	.106
3	50	.108
1	150	.092
2	150	.120
3	150	.131

Data is for 38 Hz mode. See Figure 4.5 for position of Strain Gage.

Plate Size: 42" x 42" x 1/4"
 Bolts: 4 mid-edge with washers, no center bolt
 Insulation: Kaowool, compressed to 70% of initial thickness

<u>Mode Frequency (Hz)</u>	<u>Damping Ratio x 10² (%)</u>
27	.98
55	1.93
72	1.70
125	1.91

Plate Size: 42" x 42" x 1/4"
 Bolts: 3 mid-edge with washers, no center bolt
 Insulation: Kaowool compressed to 70% of initial thickness

<u>Mode Frequency (Hz)</u>	<u>Damping Ratio x 10² (%)</u>
42	4.0
65	3.9
75	3.6
115	4.7

Plate Size: 42" x 42" x 1/4"
 Bolts: 4 mid-edge and a center bolt
 Insulation: Kaowool compressed to 70% of initial thickness

<u>Mode Frequency (Hz)</u>	<u>Damping Ratio x 10² (%) at Strain Gage</u>		
	<u>#1</u>	<u>#2</u>	<u>#3</u>
26	.89	.76	.81
86	1.80	1.00	1.40
115	2.10	.86	1.01
160	3.10	.90	1.80
220	2.20	.90	1.80
340	2.20	1.10	1.60
650	1.50	1.20	1.30
2000	1.60	1.40	1.00

Note: See Figure 4.5 for position of Strain Gages 1, 2, and 3.

Plate Size: 42" x 42" x 1/4"
 Bolts: 3 mid-edge and a center bolt
 Insulation: Kaowool compressed to 70% of initial thickness

<u>Mode Frequency (Hz)</u>	<u>Damping Ratio x 10² (%) at Strain Gage</u>		
	<u>#1</u>	<u>#2</u>	<u>#3</u>
26	1.7	1.6	1.6
42	4.6	3.2	3.2
65	2.9	4.8	3.2

100	1.0	0.87	0.60
160	1.3	0.90	1.0
200	1.1	1.6	2.0
650	2.4	1.2	2.0

Plate Size: 21" x 21" x 1/16"
 Bolts: 4 mid-edge, no center bolt
 Insulation: None

<u>Mode Frequency (Hz)</u>	<u>Damping Ratio of 10^2 (%)</u>
35	.70
120	.72

Plate Size: 21" x 21" x 1/16"
 Bolts: 3 mid-edge, no center bolt
 Insulation: None

<u>Mode Frequency (Hz)</u>	<u>Damping Ratio x 10^2 (%) at</u>		
	<u>S.G. #1</u>	<u>S.G. #4</u>	<u>LVDT #1</u>
15	3.6	4.4	6.3
32	2.5	3.9	3.4
120	3.8	4.9	2.6

Plate Size: 21" x 21" x 1/16"
 Bolts: 4 mid-edge, no center bolt
 Insulation: Kaowool compressed to 70% initial thickness

<u>Mode Frequency (Hz)</u>	<u>Damping Ratio of 10^2 (%)</u>
47	26.0
125	7.3
160	3.2
200	4.0
370	4.0
2000	4.0

Plate Size: 21" x 21" x 1/16"
 Bolts: 3 mid-edge, no center bolt
 Insulation: Kaowool compressed to 70% initial thickness

<u>Mode Frequency (Hz)</u>	<u>Damping Ratio x 10^2 (%) at Strain Gage</u>	
	<u>#1</u>	<u>#4</u>
27	22.0	29.0
36	27.0	23.0
100	3.9	7.5
160	5.0	5.6
300	2.0	2.9
600	2.3	4.7

4.5.5 Discussion of Results

The results indicate that the damping ratio in the plate is space dependent and increases close to "energy-sinks" which cause the plate to dissipate some vibratory energy locally. Bolts, nuts, and washers attaching the plate are such "sinks", actual slipping of adjacent parts accompanied by friction dissipates energy and increases the damping locally.

The addition of washers between nuts and the plate increases the contact area between attachment fixtures and the plate. It follows that friction and damping increase.

The effect of torque on bolts on the damping ratio is not clear, but it is assumed that when one increases the torque, the normal force on the moving parts increases and so does the friction and the damping ratio. At some point the torque is high enough to reduce relative slippage between nuts, washers, and the plate. Friction and the damping ratio then decrease.

Insulation compressed by the cover plate being a highly resistive medium increases the damping ratio.

When the plate vibrates in many modes, each bolt is subjected to oscillating loads. Some modes cause certain bolts to be highly loaded, while other modes cause them to be lightly loaded. The contribution to the damping of a mode from any one bolt will, therefore, vary from mode to mode.

The results reported above support the theory that the cover plate damping includes two components:

a. A bulk or material damping which absorbs energy by internal friction in the solids. This mode of damping is proportional to the square of the plate displacement and can be shown to follow the relationship:

$$\xi \approx \frac{1}{4\pi} \left(\frac{\Delta E}{E} \right) \quad (4.8)$$

where ξ is the damping ratio, E and ΔE the vibratory energy and energy absorbed per cycle, respectively.

b. A structural damping due to slippage and friction between the plate and the fixtures. This structural damping exhibits a maximum as the pressure between the sliding parts increases from no pressure to a high pressure prohibiting slippage.

In general, the following conclusions related to the cover plate behavior can be drawn from the measured values of the damping ratio of the 42" x 42" x 1/4" plate.

a. The failure of one of the four bolts attachment causes an increase of the damping ratio from an average of 0.008 to 0.016. The resulting increase in vibratory energy will be shared, in this situation, by the three remaining bolts

and consequently lead to a shorter lifetime of the plate cover. The loss of one attachment bolt causes the plate to vibrate in additional modes with added natural resonance frequencies. This situation has not yet been analyzed. It is, however, conceivable that such a shift in frequencies could cause new resonance phenomena.

b. The loss of resiliency of the kaowool insulation, or the physical loss of that insulation due to helium permeation causes the damping ratio to decrease by a factor of 10. This effect would reduce the cover plate lifetime by a factor of 20.

c. An increased friction between the cover plate and the bolt fixture, simulated by the addition of washers, increases the damping ratio ξ by 30%.

d. The loss of the central post attachment decreases the damping ratio by 20%.

Three-dimensional analysis modeling the cover plate and the attachments will have to be performed to determine the effect on the entire structure of variable damping ratio. Generally, an increased damping ratio will reduce the vibratory amplitude of the plate and increase the plate lifetime (approximately an increase by a factor of 20 of the lifetime for an increase by a factor of 10 on the damping ratio). However, an increase in the damping ratio does produce more energy storage in the attachment fixture and a consequent shorter lifetime of the bolts.

4.6 Surveillance of German Federal Republic HTR Program (L. G. Epel)

The effort begun in early 1977 to assure that the HTGR Safety Division at BNL would maintain cognizance of significant events occurring in the HTR program in the German Federal Republic was formalized during this quarter. This effort is comprised of two tasks, each of which is implemented on a continuous basis. The first of these, which is more routine and perfunctory than the second, consists of reviewing and cataloging technical reports issued by various German organizations and set to BNL under information exchange agreements. The second task is less formal but may prove to be more interesting as well as more informative; it involves perusal of abstracts, technical periodicals, and newsletters for current items concerned with the German HTR effort.

4.6.1 Technical Report Library

The cataloging of the German HTR program technical reports received through information exchange agreements is done in a computerized file and in a manner consistent with that used for the rest of the HTGR Safety Division Library. In addition to the usual processing, however, key words are generated for each report by scanning it shortly after receipt. These key words are then included in the computer file along with such items as author(s), title, reference number, publication date, etc.

Most of the formal reports received to date come under the United States-German High Temperature Reactor Research Exchange Program (reference numbers beginning with GERHTR), but many are under the United States-Euratom Fast Reactor Exchange Program (reference numbers beginning with EURFNR). In addition, several reports have been received from European organizations directly and these are treated in the same manner. Representative organizations in this category include the Kernforschungsanlage (KFA) in Julich, FRG and the Institute for Reactor Research in Wurenlingen, Switzerland. The computer file currently contains information on about 50 reports.

4.6.2 Abstracts, Journals, and Newsletters

Keeping current on the most recent developments in the German HTR program is a less organized endeavor by nature. However, it has been made somewhat systematic by stipulating that as a minimum, certain reading material be covered every month. First, an abstracting service is called every two weeks. This is the International Nuclear Information System (INIS) Atomindex, published semi-monthly by the International Atomic Energy Agency. Second, at least three technical journals are surveyed each month, including Nuclear Engineering International, Nuclear Engineering and Design, and Kerntechnik. These periodicals and the reports abstracted in Atomindex serve as a supplement to the reports cataloged in the Technical Report Library by yielding some insight into the problems being attacked by the HTR community in Europe.

The news items of most current interest are gleaned mainly from three publications which are scanned as they appear: Nuclear News, Nucleonics Week, and ATW News. The first two of these are familiar newsorgans in the United States; ATW News is a German publication which focuses on developments in the nuclear industry in Germany and is published once a month.

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II. LMFBR SAFETY EVALUATION

SUMMARY

The engineering design for the hydrodynamic dispersal test has been completed and is described. Preliminary investigations in internally heated, subassembly-like geometries indicate little or negligible liquid dispersion even at power densities significantly higher than those required to provide the minimum dispersal velocity according to well established criteria.

The first boron nitride test surface for the boiling pool experiments failed, apparently due to defective, vendor-supplied materials. A new plate has been machined and installed for pre-assembly testing. Hardware for automatic data acquisition has been installed and on-line data analysis written and debugged. Single-phase freezing results have been analyzed following completion of this phase of the work, and a final report is nearing completion.

The freezing apparatus has been modified for two-phase, gas-liquid inlet testing which is now in progress. Hardware has been installed and on-line data analysis programs written and debugged to allow on-line, automatic control, measurement, and display of experimental results to be undertaken. Visual test observations have indicated some void inclusion in the frozen layer at higher void fractions. Analysis has shown that the ratio of two-phase flow freezing rate to the single-phase case is inversely proportional to the liquid fraction of the flow for homogeneous flow with entrapped void fraction equal to the flowing void fraction. Extension of this analysis is being undertaken to determine the importance of variable frozen layer void and slip. The experiments indicate, however, a major effect of void fraction on the channel plugging time.

The progress made during this reporting period for the SSC Code Development program can be classified under two categories: code assembly and testing of stand-alone modules. In the first category, the driver routine for transient calculations was written. Some modules from stand-alone sub-programs were also interfaced. In testing of stand-alone modules, further cases for core flow redistribution model were studied successfully. An alternate method to calculate heat transfer from the liquid interfaces into the sodium vapor/fission gas bubble was developed. This method is being tested for its suitability to SSC. Major accomplishments in integrated coding of the primary and intermediate heat transport systems were also achieved.

The Code Validation effort consisted of gathering pertinent thermohydraulic tests such as those done in EBR-II reactor and the ones being planned for as preacceptance tests in FFTF. Analyses of these tests will qualify natural convection models of SSC. The sensitivity analysis work is being documented in a report. Similarly, a report on pre-analysis phase of the BIZET experiments is being prepared.

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

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

A. Experimental Investigations

Preliminary design of the test apparatus for the hydrodynamic dispersion experiments has been completed. A memorandum detailing this effort has been written, and is being circulated for discussion and criticism.

The system parameters were chosen to satisfy the following simulation requirements:

- (i) provide a simulation of two-phase flow dynamics of open boiling pools, using a non-boiling liquid with volumetrically distributed gas injection.
- (ii) simulate vapor source densities equivalent of up to 10 percent of steady LMFBR power.
- (iii) simulate subassembly scale boiling pool geometry.
- (iv) simulate the fluid dispersal conditions as predicted by the Kutateladze criterion and allow for adequate dispersal margin.

The major element of the experimental system is the test section. The simulation requirements listed above were developed into the following general test section design criteria:

- (i) Provide for volumetrically-distributed, as well as planar, injection of gas into a liquid pool.
- (ii) Simulate a volume-boiling system with local gas injection rate proportional to the local liquid fraction.
- (iii) Provide sufficient gas flow to insure a sufficiently large margin beyond the Kutateladze dispersal limit.
- (iv) The lateral dimension of the system is chosen to coincide with LMFBR subassembly dimensions.
- (v) The gas injection system is to provide minimal disturbance to the two-phase flow field.
- (vi) The gas injection rate at any axial position should be measured directly, or be otherwise known.
- (vii) The system should be capable of handling either Hg/air, H₂O/air (and possibly Hg/H₂O) fluid combinations.
- (viii) Photographic observation of the test apparatus should be easily obtainable.

- (ix) Sonic velocity conditions cannot be tolerated anywhere in the gas delivery system.

Figure 1.1 is a schematic of the proposed test section. Gas supply tubes form a horizontal grid which penetrate the test section all along the axis. The grid is composed of a number (N) of gas supply segments, with each segment containing several supply tubes. The gas supplied to each segment can be monitored and varied independently. With this capability, it is possible to inject gas with axial distributions which simulate volume-heated boiling, i.e., uniform vapor generation per unit volume of liquid.

Calculations have been performed to evaluate the number of segments, N, which need to be specified in order to approximate the void distributions characteristic of volume-boiling flow dynamics. Assuming a churn-turbulent regime, with the drift-flux given by the Zuber-Findlay correlation, steady-state void distributions were computed for

- (i) a continuous vapor source, $S(\text{cm}^3 \text{ vapor}/\text{cm}^3 \text{ liq-sec})$, proportional to the local liquid volume fraction.
- (ii) a continuous spatially uniform source of gas, $S(\text{cm}^3 \text{ vapor}/\text{cm}^3 \text{-sec})$
- (iii) a discretely distributed, segmented system of sources, distributed every 10 cm.

The system cross-section is as shown in Figure 1.1. The results, presented in Fig. 1.2, indicate that the void distribution in a pool with segmented gas sources closely approximates the distribution in a boiling system with vapor generation proportional to the local liquid fraction. The results also show, as expected, that the void distribution in systems characterized by constant vapor generation per unit total (liquid + vapor) volume does not, in general, approximate the distribution in boiling systems.

As a result of these calculations, it was decided to place independently controllable gas injection segments every 5 cm for the first 100 cm of length, and every 10 cm for the remaining 50 cm. A porous plate will be located at the bottom of the test vessel, and can be used both independently as a planar gas source, or as a gas supply for the first gas segment.

Table 1.1 presents the characteristics of the proposed test section design. This design is currently being reviewed, instrumentation concepts are being evaluated, and flow facility specifications are being developed.

1.2 Liquid Dispersion in Internally Heated Boiling Pools (T. Ginsberg)

A. Experimental Investigations

The tall boiling pool apparatus has been used to observe the qualitative behavior of internally heated boiling pools at power densities greater than those feasible with the earlier BNL apparatus (Gustavson 1976). The pool, 120 cm long, is shown in Fig. 1.3; its cross-section is shown in Fig. 1.4.

Initial efforts with the apparatus were devoted towards development of photographic techniques to best display the boiling flow characteristics.

Fig. 1.1 Schematic of Proposed Test Section

Fig. 1.3 Boiling Pool Test Section

Fig. 1.2 Void Distributions in Two-Phase Pool

Fig. 1.4 Cross-Section of Boiling Pool

Table 1.1 Test Section Specifications

Shape: square cross section

Orientation: vertical

Cross-section (flow volume) dimensions: 10 cm x 10 cm

Height (flow volume): 150 cm

Air injection modes: planar; volume-distributed

Fluid pair compatibility: water/air; mercury/air; (mercury/water)

Maximum required air flow rate: 500 scfm

Supply pressure required: 100 psi

Test section pressure rating: 15 psig

Number air supply segments: 25

Segment spacing:

5 cm for first 100 cm height

10 cm for last 50 cm height

Number gas supply tubes per segment: 4

Gas supply tube type: porous metal/plastic

Gas supply tube: O.D. = 3/8 in; I.D. = 1/4 in.

Maximum area fraction occupied by tubes: 18.75%

Volume fraction occupied by tubes: 2.5%

Maximum gas flow per segment: 30 scfm

Maximum gas velocity through porous wall: 4 ft/sec (240 scfm/ft²)

Both still-and high-speed Fastax motion picture photographic techniques have been utilized.

The boiling pool system uses the same power supply used in the previous boiling pool studies (Gustavson 1976). Zinc sulfate is also used as the electrolyte, and water is supplied to make up for vaporization.

Preliminary photographic observations are shown in Fig. 1.5, which also shows the pool power, power density (as percent of LMFBR full power density) and superficial vapor velocity at the top of the pool. The liquid level at zero power was 30 cm.

From its appearance, the pool at 1 kw was in the bubbly flow regime. The maximum superficial vapor velocity was 17 cm/sec. Assume that the steady state superficial gas velocity in the bubbly flow regime in a stagnant liquid is (Wallis 1969)

$$j_g = \left(\frac{1.18}{1.53} \right) \alpha(1 - \alpha) \frac{[\sigma g(\rho_f - \rho_g)]^{1/4}}{\rho_f^{1/2}} \quad (1.1)$$

where α is the void fraction, σ is the system surface tension, ρ_f is the liquid density, ρ_g is the vapor density. Assuming, following Wallis (1969), that a change in flow regime occurs at a void fraction of $\alpha = .4$, a value of $j_g = 4.6$ or 6.0 cm/sec is computed for the flow regime transition from bubbly to the slug flow regime. According to this prediction, a transition to the slug flow regime should have occurred somewhere within the pool at 1 kw. Although some bubble agglomeration probably occurred at the upper portion of the pool, a flow regime transition is not obvious visually. At 4.8 kw, it is apparent that the pool flow behavior is chaotic and perhaps is in the churn-turbulent flow regime.

The Kutateladze criterion (Kutateladze 1959), predicts that at a superficial vapor velocity given by,

$$\frac{j_g \rho_g^{1/2}}{\sqrt[4]{g\sigma(\rho_f - \rho_g)}} = .14 - .19 \quad (1.2)$$

a transition from a continuous liquid regime to a dispersed, vapor continuous flow regime will occur in a stagnant pool, for a water/steam system, $j_g = 94-128$ cm/sec, depending on the choice of constant. Thus, at 11 kw, a transition to the dispersed flow regime should be apparent within the pool volume. In fact, no such transition is obvious in Fig. 1.5. The liquid/vapor interface is visible at the top of the photograph, with some droplets entrained right above it.

POWER = 1kW
 $J_G = 17$ CM/SEC
 POWER DENSITY = 0.064%

POWER = 4.8 KW
 $J_G = 82$ CM/SEC
 POWER DENSITY = 0.31%

POWER = 11kW
 $J_G = 187$ CM/SEC
 POWER DENSITY = 0.72%

Fig. 1.5 Photographs of Boil-Up Pool

Nearly all the liquid is in the continuous liquid flow regime.

The above results are preliminary. More detailed observations will be made. The results, however, provide preliminary evidence that further study of boiling pool fluid dynamic characteristics is called for.

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

(a) Design:

A traversing mechanism has been obtained from excess equipment which will allow automated acquisition of pressure and temperature profiles along the vertical axis of the boiling pool. Pressure, temperature, and position sensors will be mounted on the table and the traverse will be motorized for computer control.

Sealant problems previously recognized have been corrected. The walls of the apparatus have had an o-ring installed, and the coolant channels have been sleeved for minimal leakage.

A new Boron Nitride vertical wall test plate has been machined and installed in the apparatus.

(b) Instrumentation:

The instrumentation for the experiment is nearly completed. The Cross Bar Scanner has been interfaced with the 21 MX computer and Integrating Digital Voltmeter for automated data acquisition. Software routines have been written for contact relay control to start and stop traversing equipment. Actuation of the experiment from a remote terminal has been accomplished. The 0.010 inch diameter thermocouples have been cut, polished, stripped, and wired to the connectors in preparation for the final task, gold plating.

Data acquisition and reduction software has been written and debugged, including data storage and on-line plotting. Experimentation is expected to begin upon arrival of the 150°F reference junctions.

(c) Analysis:

Analysis has begun concerning development of a model for heat transfer to an inclined surface in the presence of a two-phase boiling liquid with volumetric energy source. One phase of the analysis is an attempt to correlate the existing vertical wall heat transfer coefficient data (Gustavson 1976) to the Grashof number, $Gr(x)$, where the Grashof number is based on the local density difference across the free convective boundary layer through the local void fraction. This analysis will begin in the fourth quarter, FY 1977.

1.4 Solidification Dynamics of Flowing Core Debris (G. A. Greene, J. J. Barry)

(a) Experimental Apparatus:

The existing experimental apparatus for the single phase transient solidification task has been modified in order to investigate the solidification

of a two-component, two-phase mixture of Wood's metal and N_2 gas. The test section inlet has been modified for circumferential injection of preheated N_2 into the Wood's metal flow creating a two-phase flowing mixture. The flow is activated and terminated by a computer-controlled relay connected to a pneumatically-operated valve. The mixture exits the tube into a collecting reservoir on a load cell, which gives instantaneous readings of mass displaced from the test section. The data is scanned by an automated data acquisition system, including on-line data reduction and graphical data display.

Simultaneously, a flow visualization experiment has been performed for qualitative assessment of crust shape, flow regime, and possible void freezing.

(b) Experimental Measurements

The thermocouples are standard type-K chromel-alumel 0.020 inch diameter thermocouples. They are calibrated to within $\pm 1^\circ F$ at the ice point and steam point. The load cell is a Baldwin-Lima-Harmelton SR-4 model accurate over the range 0-25 Kg to ± 1 percent full scale.

In the Wood's metal/ N_2 experiment, the following quantities are identified and measured or calculated:

- (i) initial Wood's metal reservoir depth
- (ii) N_2 gas inlet temperature
- (iii) Wood's metal inlet temperature
- (iv) Cover pressure
- (v) N_2 gas flow rate
- (vi) Wood's metal mass displaced and flow rate
- (vii) Coolant temperature
- (viii) Plugging time
- (ix) Average void fraction

The average void fraction $\langle \alpha \rangle$, is calculated from the Zuber-Findlay correlation for homogeneous two-phase flow taking into account radial variations in the local void fraction and neglecting local slip as,

$$\langle \alpha \rangle = \frac{1}{C_0} \frac{j_g}{j_g + j_l} \quad (1.3)$$

where C_0 is the Zuber-Findlay void distribution parameter and $\bar{v}_{gj} \ll 1$. Through the experiment, j_l varies from a maximum value upon initiation of the experiment to zero at plugging. The mass flux of the gas, G_g , may be written as

$$G_g \sim \sqrt{\Delta P_g} \quad (1.4)$$

and the mass flux of the two-phase mixture is

$$G_{TP} \sim \sqrt{\Delta P_{TP} / \phi^2} \quad (1.5)$$

The ratio of these two quantities, assuming ΔP_g and ΔP_{TP} remain constant, is

$$\frac{\sqrt{\Delta P_g}}{\sqrt{\Delta P_{TP} / \phi^2}} \sim \sqrt{\phi^2} \quad (1.6)$$

where ϕ^2 is the Martinelli-Nelson two-phase multiplier. The ratio may be approximated by the quantity

$$K\sqrt{1 + \langle \alpha \rangle} = \text{constant} \quad (1.7)$$

Thus, a necessary and sufficient condition for assuming the average void fraction is represented by equation (1.3) is that both mass fluxes scale by a constant parameter, K. Preliminary observation of the flow rates indicate that this assumption is reasonable for this experiment.

(c) Data Acquisition:

The data acquisition-analysis system consists of transducers for mass, temperature, and time connected to an HP 2911 A, B Cross Bar Scanner-Controller, which feeds the raw signals to a HP 3455 High Resolution Integrating Digital Voltmeter. The experiment is controlled by an interactive software routine which initiates and terminates the data scan from a remote terminal. Data is converted and reduced on line in the time-sharing mode by a 21 MX Hewlett Packard mini-computer facility, and hard copy tabulation, as well as plotting, done on a Statos line printer.

Experiments are in progress to investigate the effect of sensible heat and average void fraction on the mass flow and time for flow blockage for a Wood's metal/ N_2 gas mixture flowing vertically down from a pressure-controlled reservoir through a circular channel (See Fig. 1.6). The tube is cooled by a double-pipe heat exchanger. This experiment is designed to simulate the conditions which may exist in the core of a LMFBR during the transition phase of a HCDA, during which it may be postulated that a two-phase mixture of molten fuel and non-condensable fission gas may be ejected through the shield or reflector flow channels.

(d) Experimental Results:

Flow visualization experiments have been performed with a water/air mixture in a double-glass condenser tube cooled by flowing alcohol. Visual observations indicated the following behavior (See Fig. 1.7):

- (i) At low void fraction, solidification of appreciable void was not observed. This indicated that the time scale of the transport of momentum in the two-phase flowing core was less than the time scale for the growth of the interface. For higher void fraction, some void entrapment was observed.
- (ii) The frozen interface was observed to be smooth and monotonically increasing in a radial fashion as a mathematical model in preparation predicts.
- (iii) The flow regime in the two-phase mixture was initially bubbly flow for low void fraction, but rapidly evolving to slug flow as $\langle \alpha \rangle$ increased.

These conditions are recognized to be void fraction dependent and are continuing to be investigated.

Transient two-phase freezing experiments of a flowing mixture of Wood's metal and N_2 gas have begun. They cover the following range of experimental parameters:

average void fraction: 0.00-0.40
 inlet temperature (Deg C): 75.0-135.0

Preliminary experimental data for mass displacement vs time at the conditions that the inlet temperature of the mixture is 80.0°C , and for $\langle \alpha \rangle$ from 0.00 to 0.40 are included. (See Figures 1.8 a-e). The data indicate a monotonically decreasing plugging time, and mass displaced prior to plugging as $\langle \alpha \rangle$ increases.

A two-phase homogeneous flow transient solidification model is being developed which indicates that the rate of freezing of a two-phase mixture is related to the freezing of a single-phase liquid through the void fraction, assuming the solid region void equals the molten core void. This relation is expressed as

$$\left. \frac{dR}{dt} \right|_{\alpha \neq 0} = \frac{1}{1 - \alpha} \left[\frac{dR}{dt} \right]_{\alpha=0} \quad (1.8)$$

The experimental results indicate that as $\langle \alpha \rangle$ increases, t_p decreases in a fashion that suggests the plugging time is bracketed between the zero void plugging time and the time predicted by assuming the solid void

Fig. 1.6 Two-Phase, Non-Condensable Vapor Transient Freezing/Streaming Apparatus (Wood's Metal/N₂ gas).

Fig. 1.7 Two-Phase, Non-Condensable Vapor Transient Freezing/Streaming Flow Visualization Apparatus (Water/air)

Fig. 1.8a Wood's Metal Mass
Displaced vs Time Till
Plugging
($T_i = 80.0^\circ\text{C}$, $\langle\alpha\rangle = 0.00$)

Fig. 1.8b ($T_i = 80.0^\circ\text{C}$, $\langle\alpha\rangle 0.09$)

Fig. 1.8c ($T_i = 80.0^\circ\text{C}$, $\langle\alpha\rangle 0.20$)

Fig. 1.8d ($T_i = 80.0^{\circ}\text{C}$, $\langle\alpha\rangle = 0.26$)

Fig. 1.8e ($T_i = 80.0^{\circ}\text{C}$, $\langle\alpha\rangle = 0.40$)

equals the liquid void,

$$t_p \leq t_p \leq t_p \quad (1.9)$$

$\alpha_s = \alpha_c \qquad \alpha = 0$

One can then conclude that significant freezing of non-condensable void fraction in the solid crust will have a predominant effect on the plugging time and mass displaced prior to plugging.

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

1.5.1 Single-Phase Experiments with Non-Melting Channel Wall

Data acquisition of paraffin wax tests with the upgraded instrumentation have been completed. A complete summary of the paraffin wax tests data along with some statistical observations are presented in Table 1.2. Included among the experiments were a number of runs repeated under nominally the same conditions for reproducibility as in the case of Wood's metal tests reported in the previous Quarterly Report (Jones 1977).

Two typical experimental results of the transient mass displacements are shown in Figures 1.9 and 1.10 along with comparisons with predictions of the three analytical methods (Gasser 1977, ElGenk 1976) of the transient solidification process. Briefly, the three analytical methods are-as follows:

Gasser (1977) developed a model for the transient solidification process which accounts for the transient axial variation of solid-layer growth rate and the feedback of solid-layer growth on channel fluid flow behavior. This model neglects all heat capacity effects in both the solid layer and in the bulk liquid. The solid layer growth rate, according to this model is given by

$$\frac{dR}{dt} = \frac{h_i(T_n - T_f) \left(k_s + R_o U \ln \frac{R_o}{R} \right) - \frac{R_o U k_s}{R} (T_f - T_\infty)}{\rho \lambda \left(k_s + R_o U \ln \frac{R_o}{R} \right)} \quad (1.10)$$

It has been proposed that a first-order approach to the effect of solid-layer heat capacity may be made by assuming that, in addition to removal of latent heat of fusion at the interface, energy is also removed to reduce the solidifying liquid to the average solid-layer temperature. With this modification, the solid-layer growth rate is

$$\frac{dR}{dt} = \frac{h_i(T_n - T_f) \left(k_s + R_o U \ln \frac{R_o}{R} \right) \frac{R_o U k_s}{R} (T_f - T_\infty)}{\rho [\lambda + C_s(T_f - \bar{T}_s)] \left(k_s + R_o U \ln \frac{R_o}{R} \right)} \quad (1.11)$$

This equation was used to replace Equation 1.10 in the computer analysis model presented by Gasser (1977).

More recently, a simplified model has been developed in which the axial variation of the solid-layer growth is not considered. The model assumes that the liquid bulk temperature and velocity at the end of the channel are constant. The assumption of constant velocity is removed, however, in the computation of transient mass displacement; the transient mass displacements are given by

$$M(t) = \frac{\pi R_2^2}{4} \rho_l V_2(R_2) \Delta t \quad (1.12)$$

Thus, V_2 is not assumed to be constant in Eq. (1.12). It is assumed to be constant only in the evaluation of Δt which is given by the integration of the following equation from R_o to R :

$$\frac{dR}{dt} = \frac{h_i(T_2 - T_f) \left(k_s + R_o U \ln \frac{R_o}{R} \right) \frac{R_o U k_s}{R} (T_f - T_\infty)}{\rho \lambda \left(k_s + R_o U \ln \frac{R_o}{R} \right)} \quad (1.13)$$

Three independent calculations were performed. The first two used Gasser and Kazimi's model with Eq. (1.10) and Eq. (1.11), respectively, as the basic growth rate relations. These are here called the "Existing Model" and the "Existing Model with $(C_p \Delta T)_s$ ". The third calculation was performed using the "simplified model" (described above) given basically by Equations (1.13 and (1.12)

The results show reasonable agreement between the data and the three analytical predictions.

Nomenclature

C_s	Specific heat of the frozen layer
h_i	Inside heat transfer coefficient
k_s	Thermal conductivity of frozen layer
R	Radius of frozen layer crust
R_o	Initial tube radius
T_f	Freezing point
T_n	Temperature of flowing liquid at node-n
T_∞	Ambient temperature
U	Overall conductance
λ	Latent heat of fusion
ρ	Density

Table 1.2
Test Conditions and Experimental Results of Paraffin Wax Tests with Upgraded Instrumentation

Test Conditions						Experimental Results						
Run No.		Initial Paraffin Wax Temp. (oC)	Coolant Inlet Temp. (oC)	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)
1	A	55	15.6	13.6	13.2	33.5	34.1	3.7	22.0	20.9	21.6	1.4
	B		15.6		13.2	30.8			21.9	20.8		
	C		15.6		13.0	38.1			24.3	23.2		
2	A	65	15.6	13.6	12.5	104.6	89.7	15.6	38.5	37.8	34.4	2.7
	B		15.3		12.7	101.6			35.5	34.8		
	C		15.3		12.7	75.6			34.2	33.5		
	D		16.1		12.7	76.9			32.1	31.4		
3	A	75	15.8	13.6	12.4	110.0	111.9	2.5	44.8	44.1	43.3	0.7
	B		15.8		12.4	110.9			43.7	43.0		
	C		15.6		12.3	114.7			43.4	42.7		
4	A	85	15.6	13.6	12.1	129.4	175.6	40.7	51.2	50.5	65.6	13.5
	B		15.6		11.8	191.6			70.3	69.6		
	C		15.6		---	205.9			77.2	76.5		
5	A	95	15.6	13.6	----	261.1	346.1	147.2	91.2	90.5	123.6	39.3
	B		15.8		---	261.1			113.9	113.2		
	C		17.2		----	516.0			167.7	167.0

Fig. 1.9 Reproducibility and Comparison with Theory for Paraffin Wax Inlet Temperature of (55°C)

Fig. 1.10 Reproducibility and Comparison with Theory for Paraffin Wax Inlet Temperature of (95°C)

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

A survey and review program for the application of fracture mechanics methods in elevated temperature design analysis and safety evaluation was initiated in December 1976. The first report surveyed and assembled the material for the critical review of the theories of fracture and the application of fracture mechanics methods to life prediction and safety analysis of piping components. During this report period, a second report providing basic concepts and a review of the problem areas associated with the development of analytical and experimental programs for a systematic evaluation and comparison of the currently available fracture mechanics theories was issued (BNL-NUREG 50676). The basis for such an evaluation is conceived as a series of benchmark problems which are accurately specified examples of geometry, loading, and environmental conditions, characteristic of large diameter thin wall piping systems in nuclear service. Starting from the simplest test coupons for cracked plate specimens, the program is to be designed in such a way that the range or validity and relative merit of the competing assessment methods can be evaluated and the results can be applied to increasingly more complex test configurations and ultimately to real piping systems.

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

The Super System Code (SSC) program deals with the development of an advanced thermohydraulic 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 both safety and design or operational transients. Reference is made to the previous quarterly progress report (Agrawal, 1977a) for a summary of accomplishments prior to the start of the current period.

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

In a loop-type LMFBR, a fraction of the total coolant flow into the reactor vessel is allowed to pass between the thermal liner and the vessel wall. The purpose of this bypass flow is to protect the vessel wall from excessive thermal transients. During a transient, such as a normal scram, it was found (Yang, 1977) that the bypass flow plays an important role on flow redistribution in the core. An investigation, therefore, was carried out to determine the overall effect of bypass flow on the entire reactor vessel in such a transient. The investigation includes the upper outlet plenum, the core region and the lower inlet plenum. Numerical results for representative cases were obtained and are reported.

In the analysis, the bypass channel was divided into two axial sections: the lower section adjacent to the active core region and the upper section separated from the outlet plenum by the thermal liner. The lower section was allowed to have heat generation due to gamma heating; the upper section did not have internal heat generation, but heat transfer through the thermal liner was considered. The reactor core was represented by ten channels, each of which was given different steady-state heating and flow rates. The bypass channel interacts with the ten core channels according to the transient models (Yang, 1977 and 1976). One-dimensional, single-phase, incompressible flow equations were used to compute the flow rate, pressure drop and enthalpy change in the two sections.

The results of the analyses indicate that for the transient considered, the average sodium temperature is reduced by 11 K at steady state when bypass flow at the top of the plenum is included. This temperature difference affects the flow stratification occurring during the transient. Figure 3.1 shows the effect of bypass flow on flow stratification and outlet nozzle sodium temperature. It can be seen that both the degree and duration of stratification are reduced when bypass flow is considered and that the resulting outlet nozzle sodium temperature is reduced. The reduction of outlet nozzle temperature has a feedback effect on the vessel through the IHX of the primary loop. Since the performance of the IHX is slightly improved under pump coastdown conditions, (Gunby, 1970) it is expected that the temperature at the inlet nozzle will be further reduced.

In the core region, it has been shown (Yang, 1977) that due to the lack of buoyancy forces in the bypass channel, much of the bypass flow is drawn into the core which has a much higher heating rate and higher buoyancy force. Consequently, temperatures in the core region are reduced with the addition of flow

Fig. 3.1. Effect of bypass flow on mixing in upper outlet plenum.

from the bypass channel, as shown in Fig. 3.2, in which two channels representing a fuel assembly and a radial blanket assembly are illustrated.

The bypass flow can have both indirect and direct effects on the lower inlet plenum. The indirect effect is the feedback on the inlet nozzle temperature from the upper plenum as discussed above. The direct effect is related to the flow reversal in the bypass channel. During the period of flow reversal, the bypass flow, which is at a slightly higher temperature, enters the lower plenum and is mixed with the fluid stored in the plenum according to a complete mixing model used in the analysis; numerical results to date show that no significant effect on the average fluid temperature is obtained.

In conclusion, mathematical models for the interaction of bypass flow and the coolant flow of the entire vessel were developed. For a normal scram case, it was found that the predominant effect of bypass flow is to lower the coolant temperatures in both the core region and the outlet plenum.

3.2 Sodium Boiling and Fission Gas Release

3.2.1 Unified Model (V. Quan)

The model for fission gas release and sodium boiling has been described earlier (Agrawal, 1977b). The equations describing the bubble behavior consist of a set of ordinary differential equations and algebraic relations. A stand-alone test computer program to solve the governing equations was coded and checked out during the preceding quarter (Agrawal, 1977a).

During the present reporting period, work was initiated to couple the bubble model to the core heat transfer and system fluid flow. The computation procedure is as follows. At a new time point, the boundary conditions required to calculate the bubble properties are predicted based on the values of previous time points. These boundary conditions are the channel inlet and outlet pressures, the channel wall temperature distributions, and the liquid film thickness distributions. The bubble properties, followed by the temperature distributions in the liquid slugs above and below the bubble, are then calculated. Having computed the bubble and liquid slug properties, the boundary conditions are updated by solving the heat conduction equations for the fuel pin and liquid film and the fluid flow equations for the system.

3.2.2 Sodium Boiling (V. Quan and J. W. Yang)

The heat flux between the liquid sodium and a vapor bubble at the liquid-vapor interface for slug-annular flow is a dominant parameter in determining the initial phase of bubble growth in a reactor coolant channel under accident conditions. A solution for this heat flux has been obtained by Cronenberg (1971). In numerical application of the solution, however, complications are encountered and simplifying assumptions are imposed (Dunn, 1974). In the present work, a solution by a combination of superposition and integral technique is obtained. The solution is simple to evaluate, and it alleviates certain assumptions previously imposed.

The heat transfer equation for the liquid sodium in Lagrangian coordinates is

Fig. 3.2. Effect of bypass flow on maximum coolant temperature in core.

$$\rho c \frac{\partial T}{\partial t} = k \frac{\partial^2 T}{\partial \xi^2} + Q(\xi, t) \quad (3.1)$$

where T , t , ξ , ρ , c , k , and Q denote temperature, time, distance from liquid-bubble interface, density, specific heat, conductivity, and heat input rate, respectively. The initial condition (when boiling begins) is $T(\xi, 0) = T_1(\xi)$. The boundary conditions are $T(0, t) = T_o(t)$ and $T(\infty, t) = T_\infty(t)$.

The solution for the heat flux at the surface (liquid-bubble interface) as obtained by Cronenberg (1971) using the Laplace transformation method is given by

$$\begin{aligned} \left. \frac{dT}{d\xi} \right|_0 &= - \int_0^t \frac{1}{[\pi\alpha(t-\tau)]^{1/2}} \frac{dT_o}{d\tau} d\tau - \frac{T_o(0)}{(\pi\alpha t)^{1/2}} \\ &+ \int_0^\infty \int_0^t \frac{\xi}{2k[\pi\alpha(t-\tau)^3]^{1/2}} \exp\left[\frac{-\xi^2}{4\alpha(t-\tau)}\right] Q(\xi, \tau) d\tau d\xi \\ &+ \int_0^\infty \frac{T_1(\xi) \xi}{2\alpha(\pi\alpha t^3)^{1/2}} \exp\left(\frac{-\xi^2}{4\alpha t}\right) d\xi \end{aligned} \quad (3.2)$$

where the thermal diffusivity is $\alpha = k/(\rho c)$, and the subscript o denotes $\xi = 0$ (the liquid-vapor interface).

In general, Q is given by

$$Q(\xi, t) = Q_g(\xi, t) + \gamma h(t) [T_w(\xi, t) - T(\xi, t)] \quad (3.3)$$

where Q_g is a given heat generation term, γ is the wall surface to liquid volume ratio, h is the heat transfer coefficient, and T_w is the channel wall temperature. Since the temperature distribution $T(\xi, t)$ is not known a priori and is not computed, approximations are made for the evaluation of the second integral in Eq. (3.2). It has been taken by others (Dunn, 1974) in evaluating Eq. (3.3) that $Q_g(\xi, t) = Q_g(0, t)$, $T_w(\xi, t) = T_w(0, t)$, and $T(\xi, t) = [T_o(t) + T_c(t)]/2$ where T_c is the liquid temperature far enough from the interface that it is not affected by conduction through the interface. Further approximations, such as $T_1(\xi) = T_1(0)$, have also been imposed in numerical evaluation (Dunn, 1974) of the integrals in Eq. (3.2).

A useful technique in obtaining approximate but simple solution to heat transfer problems is the integral method (Goodman, 1964). However, as discussed by Goodman (1964), the integral method is not directly applicable to problems having arbitrary initial temperature distributions. It is observed, on the other hand, that the initial temperature distribution can be accounted for by a

solution which neglects the axial heat conduction. Therefore, in the present work, a combination of superposition and integral approach is employed as follows to obtain a solution.

Let

$$T = T_1 + T_2 \quad (3.4)$$

and

$$\frac{\partial T_1}{\partial t} = \alpha \frac{\partial^2 T_1}{\partial \xi^2} + q_g(\xi, t) + v(t) [T_w(\xi, t) - T_1] \quad (3.5)$$

where $q_g = Q_g/(\rho c)$ and $v = \gamma h/(\rho c)$. Let T_1 correspond to a solution for which axial conduction is negligible ($\partial^2 T_1 / \partial \xi^2 = 0$). One then obtains

$$T_1 = F^{-1}(t) \left\{ T_1(\xi) + \int_0^t F(\tau) [v(\tau) T_w(\xi, \tau) + q_g(\xi, \tau)] d\tau \right\} \quad (3.6)$$

where $F^{-1}(t) = \exp \left[- \int_0^t v(\tau) d\tau \right]$.

From Eqs. (3.1, 3.3-3.5), one obtains

$$\frac{\partial T_2}{\partial t} = \alpha \frac{\partial^2 T_2}{\partial \xi^2} - v(t) T_2 \quad (3.7)$$

with $T_2(\xi, 0) = 0$, $T_2(0, t) = T_o(t) - T_1(0, t) \equiv \theta(t)$, and $T_2(\infty, t) = 0$. According to the integral technique, (Goodman, 1964) one may take

$$T_2 = \theta \exp(-\xi/b) \quad (3.8)$$

where the distance scale b is a measure of the thermal boundary layer thickness. Substituting T_2 as given by Eq. (3.8) into Eq. (3.7), integrating Eq. (3.7) from $\xi = 0$ to $\xi = \infty$, and using the initial and boundary conditions for T_2 , one obtains

$$b = \left[2\alpha \theta^{-2}(t) G^{-1}(t) \int_0^t \theta^2(\tau) G(\tau) d\tau \right]^{1/2} \quad (3.9)$$

where $G^{-1}(t) = \exp \left[- \int_0^t 2v(\tau) d\tau \right]$. If a polynomial profile instead of the exponential profile as given by Eq. (3.8) is used for T_2 , a solution similar to Eq. (3.9) is obtained for the thermal boundary layer thickness.

The surface temperature gradients, which yield the heat flux at the liquid-vapor interface, are

$$\left. \frac{\partial T_1}{\partial \xi} \right|_0 = F^{-1}(t) \left\{ \left. \frac{dT_1}{d\xi} \right|_0 + \int_0^t F(\tau) \left[v(\tau) \left. \frac{\partial T_w}{\partial \xi} \right|_0 + \left. \frac{\partial q_g}{\partial \xi} \right|_0 \right] d\tau \right\} \quad (3.10)$$

$$\left. \frac{\partial T_2}{\partial \xi} \right|_0 = - \frac{\theta(t)}{b(t)} \quad (3.11)$$

It may be noted that $(\partial T_1 / \partial \xi)_0$ is generally small compared to $(\partial T_2 / \partial \xi)_0$. The main effect of T_1 on heat transfer is through θ , which appears in T_2 and $\partial T_2 / \partial \xi$.

The physical interpretations for T_1 and T_2 are as follows. The T_1 is the solution when axial heat conduction is neglected. The neglect of axial conduction is indeed generally assumed up to the time when boiling starts. Even after boiling is initiated, the effect of heat transfer to the bubble (i.e., axial conduction) is neglected in computing the liquid temperature distribution (Dunn, 1974). Hence, T_1 accounts for the effects of initial temperature distribution, heat generation, and wall heat transfer; and T_2 represents the effects of heat transfer in the thermal boundary layer.

The integral solution T_2 reduces to one obtained previously (Goodman, 1964) when θ and v are constant. The previous solution has been shown to be an accurate approximation of an exact solution. The superposed solution T_1 requires only the condition that the axial conduction term be negligible for heat transfer in the liquid in the absence of liquid-vapor heat transfer. This condition is generally assumed and reasonably accepted (Dunn, 1974). Therefore, the present solution is reasonably expected to yield accurate results over a wide range of conditions. For a sample limiting case considered, for which heat input rate and initial temperature distributions are uniform and surface temperature decreases linearly with time, it is found that the present solution and an exact solution have identical dependency on all parameters and differ only in the constant coefficient by a few percent. The advantages of the present method are that the solution is simple to compute, particularly for complicated systems, and that it alleviates certain limitations previously imposed.

3.3 Heat Transport System

3.3.1 System Hydraulics (I. K. Madni)

The hydraulic model for the primary system described in the last quarterly report (Agrawal, 1977a) was tested for four different break locations. Stable solutions were obtained in all cases. An example of flow transients resulting from a break at vessel inlet is illustrated in Fig. 3.3.

3.3.2 Check Valve (I. K. Madni)

The modeling approach for the check valve is similar to that employed in RELAP (1974). The flow-dependent pressure losses are assumed to be represented by:

Fig. 3.3. Coolant flow transients for pipe-break

$$\Delta P_{CV} = c_1 \frac{W|W|}{\rho_{CV}} \quad (3.12)$$

The model allows for three regions of operation for the valves, hence, three pressure loss coefficients

- (i) c_1 for positive flow with valve open
- (ii) c_2 for negative flow with valve open
- (iii) c_3 for negative flow with valve in closed position.

c_1 , c_2 , c_3 are user-specified constants, in addition to P_{CV} , the back pressure required to close the valve. The code, however, has default values for these built in.

On forward flow, the valve remains open at all times. On reverse flow, the valve remains open as long as ΔP_{CV} with $c_1 = c_2$ is less than P_{CV} . So far, both valves behave identically, but from the closed position, type 1 valve opens when ΔP_{CV} with $c_1 = c_2$ is less than P_{CV} , whereas type 2 valve opens when ΔP_{CV} with $c_1 = c_3$ is less than P_{CV} . At the user's option, the valve can also remain open in the failed position on reverse flow.

The model described above has been coded for inclusion into the overall Heat Transport System module.

3.3.3 Intermediate Heat Exchanger (I. K. Madni)

The nodal diagram, essential features, heat transfer coefficients, etc. of the IHX thermal model have been described in an earlier report (Agrawal, 1976). Here, only the transient equations in the active heat transfer region will be presented. These are:

Primary coolant:

$$\rho V \frac{d}{dt} (e_{p_{i+1}}) = W'_p (e_{p_i} - e_{p_{i+1}}) - (UA)_{pt} [T_{p_{ii+1}} - T_{t_i}] - (UA)_{psh} [T_{p_{ii+1}} - T_{sh_i}] \quad (3.13)$$

Secondary coolant:

$$\rho V \frac{d}{dt} (e_{s_i}) = W_s (e_{s_{i+1}} - e_{s_i}) + (UA)_{st} [T_{t_i} - T_{s_{ii+1}}] \quad (3.14)$$

Tube wall:

$$(M_t C_t) \frac{d}{dt} T_{t_i} = (UA)_{pt} [T_{p_{ii+1}} - T_{t_i}] - (UA)_{st} [T_{t_i} - T_{s_{ii+1}}] \quad (3.15)$$

Shell wall:

$$\left(M_{sh} C_{sh} \right) \frac{d}{dt} T_{sh_i} = (UA)_{psh} \left[T_{p_{ii+1}} - T_{sh_i} \right] \quad (3.16)$$

Equations (3.13)-(3.16), as shown above, are suitable for counterflow arrangement, positive flow only. However, they have been coded with general node counters to allow for reverse flow in either coolant-stream, as also a choice of parallel or counterflow arrangement.

The equations are integrated by a fully implicit single-layer scheme. The heat flux terms in Eqs. (3.13) and (3.14) are allowed to be determined explicitly, which uncouples the equations, allowing them to be solved in a marching fashion without resorting to matrix inversion.

The hydraulic model essentially calculates the individual pressure loss components due to friction, gravity, acceleration, area changes, and other losses, and then sums them up to give the total pressure loss in both primary and secondary sides of the heat exchange unit. The same approach has allowed formulation of hydraulics in the shell (sodium) side of the superheater and evaporator as well.

3.3.4 HTS Module Development (I. K. Madni and E. G. Cazzoli)

A generalized module describing transient hydraulics in the primary and intermediate heat transport systems has been prepared. It consists of detailed descriptions of all important components such as pumps, IHX, piping, check valve, etc. and a host of user-oriented options on geometric configuration, arrangement of components, and accident information. The code has been debugged and tested under both pump coastdown and pipe break conditions.

3.3.5 Pipe Break (I. K. Madni and E. G. Cazzoli)

The detailed break model equations described in an earlier quarterly report (Agrawal, 1977b) are being programmed for inclusion into the HTS module. A routine has been developed to solve the equation for v_j , the velocity of jet out of the break.

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

The work during this quarter may be divided into three categories. The first category is changes made to the model equations in the steady-state portion of the code. The second is changes and additions to the steady-state portion of the code to interface it with the transient portion of the code. The third and last category is the coding effort on the transient portion of the code.

3.4.1 Changes to the Model Equations

An additional term was added to the pressure drop calculation. This term represents the reversible portion of the pressure change associated with the area change at the junction of two pipes of different diameters. This term is also used in the calculation of the pressure drop in a "tee" junction.

3.4.2 Interfacing Steady State and Transient

A substantial portion of the effort during this quarter was concerned with additions and changes to the steady-state portion of the code needed to interface it with the transient portion of the code. Many auxiliary variables not needed during the calculation of the steady state, but which will be used by the transient portion of the code, had to be added to the steady-state portion of the code. Most of these auxiliary variables are pointers needed by the variably dimensioned data management scheme adopted for the SSC code.

3.4.3 Transient Coding

The coding and debugging of the transient portion of the steam generator model is continuing. The momentum equation was reformulated in terms of flow rate instead of mass flow rate as originally programmed. This required coding changes in the flow redistribution calculational modules. The modified code has been compiled and is in the process of being debugged. The initial conditions required by the transient calculation are being generated by the revised steady-state portion of the code.

3.5 System Integration Method (J. G. Guppy)

Coding and testing of the transient driver module (MAIN9T) for SSC-L was completed during this quarter. As discussed in detail in the previous quarterly (Agrawal, 1977a), the multi-step scheme (MSS) is used for the advancement in time of the system components/processes. The same logic and time stepping hierarchy were used in developing the MAIN9T driver. Test cases were run with MAIN9T and previous results duplicated.

The interfacing of MAIN9T with the transient fuel heat conduction module (FUEL5T) is now proceeding.

For the explicit integration of the loop hydraulic equations, a fourth order, modified Runge-Kutta package (Lucas, 1976) will be employed for the first version of SSC-L. A key consideration in this decision is the lower fixed storage which this package requires compared to more sophisticated integration methods. Test cases run for sample pump coastdown and pipe break transients show that the numerical accuracy with the Runge-Kutta package is in excellent agreement with higher order methods.

3.6 Code Development (J. G. Guppy, R. J. Kennett, T. Nepsee and E. S. Srinivasan)

Further debugging and refinement of the SSC-L variably dimensioned steady-state package was continued this quarter. A multi-loop case was successfully run. Various restart options are being tested.

The first draft version of the SSC-wide data dictionary was produced. This dictionary contains all global variables and their definitions. It also indicates where each variable is referenced and set and, if it is an input quantity, where it is read.

PUBLICATIONS

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

AGRAWAL, A. K., "Thermohydraulic Transient Simulation of LMFBR Systems," Presented at the Seventh Liquid Metal Boiling Working Group Meeting, June 1-3, 1977, Petten, The Netherlands.

QUAN, V. and AGRAWAL, A. K., "A Unified Model for Fission Gas Release and Sodium Boiling in Reactor Coolant Channel," Trans. Am. Nucl. Soc. 26, 345 (1977).

YANG, J. W. and AGRAWAL, A. K., "Buoyancy-Induced Flow Redistribution in Reactor Vessel of an LMFBR," Trans. Am. Nucl. Soc. 26, 450 (1977).

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

The Code Validation work this quarter covers the following areas:

- 4.1 Analysis of Integral Thermohydraulic Experiments
- 4.2 Sensitivity Analysis Methods
- 4.3 Critical Experiment Analysis

The Analysis of Integral Thermohydraulic Experiments covers the evaluation of proposed experiments, such as the preacceptance tests in FFTF, and analysis of natural convection tests that were performed in the EBR-II reactor. The Sensitivity Analysis work covers the use of statistical methods for SSC validation, and the Critical Experiment Analysis covers the design of the RSR experiments in the BIZET series on the UKAEA-Winfrith critical facility ZEBRA.

4.1 Analysis of Integral Thermohydraulic Experiments (A. K. Agrawal)

Removal of decay heat (after-heat) from an LMFBR plant through natural convection in a satisfactory and acceptable manner is a major safety consideration. The simulation of this transient is one of the prime objectives of the SSC Code Development program. (See the previous section in this progress report.) The validation of various models that are required in simulating this transient is considered to be one of the key issues. Work was, therefore, initiated to perform a systematic study of those tests that have been done, or are being planned for, which will provide validation of key models in the SSC code. Some of the most direct tests that will provide confidence in modeling include (1) the natural convection simulation experiments of EBR-II with XX07 instrumented subassembly (September 1974-March 1975), (2) the preacceptance test in support of FFTF (Fast Flux Test Facility), and (3) the forthcoming tests in EBR-II with XX08 instrumented subassembly (Fall 1977).

Initial discussions with staff members involved with these tests at Argonne National Laboratory and Hanford Engineering Development Laboratory have been started. Pre- and post-analyses of these tests as well as details will be reported later on.

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

During this quarter, Sensitivity Analysis Studies have concentrated on the numerical experiment phase of code validation. The random balance type methods outlined previously (Agrawal, 1977) have been evaluated for simple situations and are being applied to the DEMODA4 to check the feasibility and applicability of the method. Numerical experiments have been supplemented with theoretical studies.

In order to evaluate the random balance methods, Huang's (1973) codes have been used and subroutines written to produce experimental design matrices according to random and two types of orthogonal designs. A simple subroutine to calculate the statistics of the correlations has also been written together with a subroutine to decode a two-level matrix to expose its basic structure.

Some numerical experiments were performed to test the effectiveness of the method on a linear "black box" containing as many as 500 variables. Computerized

plots were obtained showing the dependence of the number of "effective" variables in subsets of variables of increasing size. The results indicate that even for the worst case (i.e., the actual values were sampled from a normal distribution), the true expected values could be determined within a constant error band provided a sufficiently large number of runs were made.

In order to gain further insight into the random balance approach, an analytical investigation of the method has been initiated and is presently being pursued. The problem reduces to the solution of a system of n linear inhomogeneous equations with random coefficients with m unknowns, where $m \gg n$, i.e., we are dealing with a randomized linear, under-determined system. Since the aim is to determine the effective variables and not necessarily their associated sensitivities, the accuracy of the results is important only to the extent that it affects the ranking of the variables according to their degree of effectiveness. This implies, for instance, that the sensitivities need be found only up to a constant multiplicative factor.

Since the system is highly undetermined, the sensitivities are not uniquely determined by the linear equations. However, using some theorems from rectangular matrix theory, it is possible to express the general solutions as a particular solution plus a solution of the homogeneous set. The particular solution will depend on the output quantities, while the solution of the homogeneous equations will be infinite in number and independent of the output quantities. Although a solution in the second category can make a significant contribution to the overall magnitude of the total solution, it contains no information concerning the relative ranking of the unknown according to magnitude and may, therefore, be omitted in determining the effectiveness of the variables.

The above ideas are currently being analyzed analytically and the results are being incorporated in a topical report. This report will complete the sub-task on evaluation of methods for sensitivity analysis.

4.3 Critical Experiment Analysis (H. Ludewig)

The preliminary analysis of a series of safety related critical experiments to be carried out on the ZEBRA facility at the UKAEA-Winfrith was completed. Analysis techniques outlined in the previous quarterly report (Agrawal, 1977) were used to complete the analysis.

The proposed experimental program is to be completed in approximately 40 days. Due to the brevity of the program, only centrally located compactions will be considered. In order to carry out the proposed experiments, the central control rod will be removed and the remaining six will be rearranged. This will result in a comparatively clean central region of the reactor for simulating the compaction. A total of three different compacted configurations will be considered: First, a compaction towards the center of the core (slumped-in); second, a compaction along the core-blanket interface (slumped-out); and finally, a compaction downward (slumped-down). In all cases, the compaction ratio is 2 to 1.

A maximum reactivity change of approximately +\$4.5 was determined for the largest slumped-in configuration. This reactivity change would be sufficient for checking perturbation Monte Carlo methods, as well as the usual methods such

as DSN and diffusion theory. Higher density compactions, which may be possible, would further increase the reactivity change, making these checks of Monte Carlo methods even more feasible. In the proposed experimental program, a detailed series of measurements will only be carried out for the base case and the slumped-in configuration with the maximum reactivity swing. Only in one configuration will all three different compaction configurations be studied. In this way, the scaling between the compaction types can be investigated. Finally, it is believed that this BIZET-B series will provide additional, valuable experimental information since they extend the data base in the direction of commercial-sized breeder reactors. These experiments are necessary to validate and extend reactor physics techniques to commercial-sized reactors, and will then be used as an aid to answer the various safety and licensing questions concerning large LMFBRs. A report outlining these results is being prepared.

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5. FRS Reliability Assessment (Ioannia A. Papazoglou)

The work in this area is being concluded. A final report summarizing the effort for the past year has been written and will be issued shortly.

III. LIGHT WATER REACTOR SAFETY

SUMMARY

The model for post-dryout, non-equilibrium vapor generation has been successfully checked against considerable other data than were used in its development. A model for flashing has been developed having one arbitrary coefficient yet to be determined. This work included separate modeling for interfacial area density and for net interfacial heat flux.

Loop construction of the test facility has continued, and the first check-out of the system begun by circulation in existing components. The test section and gamma densitometer design layouts are complete and detail drawing in progress. Considerable effort is being applied to reduce the cost of these items by continual discussions with vendors regarding particular design aspects. Work is continuing on the development of the longitudinal probe seal, and determination of the potential for flutter in the probe rake.

Simple methods have been devised for the determination of time incoherencies in recording devices utilized in parallel and for the reconstruction of attenuated and/or phase shifted signals. Verification of these methods awaits receipt of the high speed data acquisition hardware, now scheduled for receipt in mid-July. Production techniques for the optical probes have been developed, and several probes made and tested. Considerable materials development work has been accomplished for both the glass fibres themselves and the epoxy sealing materials. Experimental determination of the probe behavior has begun, following construction of small test facilities. Preliminary, very reproducible results, show the rise times upon bubble entrance to be well correlated in terms of the interface passage time. On the other hand, a velocity effect has been found in the amplitude of the signal output which has heretofore not been reported. This represents a potentially serious problem due to the resultant low amplitude and low signal-to-noise ratios at quite modest velocities.

In general, the program is on schedule and progress is occurring as expected. It is possible, however, that progress may be impeded due to extended construction of the test section and instrumentation development considerably beyond that expected.

Work on the THOR code development continued in the following areas:

Work on the demonstration test cases has been completed. The conclusions are that coupling of lumped parameter components is feasible, the lumped model showed very good accuracy and high computing speed.

Formulation of the single loop PWR blowdown has been completed. Programming of the equations has been completed and debugging is in progress. A strategy for the solution of the system of seventy non-linear system balance equations has been developed and is being tested. A subroutine, DERIV, has been developed which includes all but one of the component subroutines required for the single loop blowdown.

Coupling of a distributed parameter pipe with a lumped parameter pipe has been achieved for the case of the distributed pipe located downstream of the lumped pipe. This method is being extended for application in different flow situations and geometries.

A test case has been selected for simulation studies using the single loop blowdown code. It is the 800 series semi-scale tests performed by INEL.

Work on component models required for the two loop PWR blowdown has continued. A semi-empirical model of the downcomer has been developed which uses the Wallis' flooding correlation. For the core, a method of tracking flow regime interfaces based on kinematic shocks has been formulated, firstly for a two-region core based on non-homogeneous equilibrium flow. The six region core module has been successfully executed for HEM flow and fixed zone boundaries. A program for tracking disappearances and reappearances of regime boundaries has been written and is being tested.

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)

Previous progress reports have described the modeling undertaken to adequately describe the effects of heat flux on the non-equilibrium vapor generation rates as applied to post-CHF dispersed liquid flows. The correlation developed has now been applied to a wide range of data sets with encouraging results. Typical of the comparisons with the data base used in the development of the correlation are shown in Figures 1.1 and 1.2. Comparison in Fig. 1.1b represents a worst case for the low pressure ($p_r = 0.05$) nitrogen data of Forslund (1966), whereas Fig. 1.2b represents the worst case comparison with the Harwell data (Bennett 1967). Typical comparisons with the data of Janssen (1975) and Swenson (1967) are shown in Figures 1.3 and 1.4. The latter represents an extrapolation by a factor of 300 percent over the pressure range used in the data base. Efforts are currently in progress to check the correlation against one-dimensional calculations in rod bundles.

(b) Effect of Pressure Change (P. Saha)

The volumetric mass-rate of vapor generation for any heat transfer dominated phase change process can be expressed by

$$\Gamma_v = \frac{A_s \dot{q}''_i}{\Delta i_{fg}} \quad (1.1)$$

where A_s is the vapor-liquid interfacial area per unit volume, $\dot{q}''_i$ is the net interfacial heat flux, and Δi_{fg} is the latent heat of vaporization. For rapid depressurization, or flashing flow as applied to the problem of two-phase critical flow, the liquid becomes superheated, and supplies the thermal energy needed for vaporization. As equation (1.1) shows, both the interfacial area density and the net interfacial heat flux are of importance, and must be considered with equal emphasis.

Interfacial Area Density, A_s :

It is most likely that in a commercial pipe, as used in a reactor system, bubble nucleation will start at the wall micro-cavities with pre-existing gas phase. As the bubbles grow, they would leave the surface and move into the core. Thus, at low void fraction, a bubbly two-phase mixture can be expected. As vapor generation continues, some of the bubbles may coalesce to form a larger bubble (or a slug) and thus produce a bubbly-slug regime. At higher void fractions, the flow is expected to change into a droplet-annular flow regime, and may even be transformed into a dispersed-droplet regime as the void fraction approaches unity. The interfacial area density will also change with the flow regime, or the void fraction. Apart from the flow regime, the interfacial area density may also depend on the number of nucleation sites

Fig. 1.1 Typical comparison of calculated wall temperature with the measured values for the nitrogen data base ($p_r = 0.05$)

a) $G \sim 95 \text{ kg/m}^2\text{s}$

b) $G \sim 95 \text{ kg/m}^2\text{s}$

c) $G = 175 \text{ kg/m}^2\text{s}$

d) $G \sim 260 \text{ kg/m}^2\text{s}$

Fig. 1.2 Typical comparison of
calculated wall temperature with the
measured values for the water data
base ($P_r = 0.31$)

a) $G \sim 400 \text{ kg/m}^2 \text{ s}$

b) $G \sim 660 \text{ kg/m}^2 \text{ s}$

c) $G \sim 1360 \text{ kg/m}^2 \text{ s}$

d) $G \sim 3800 \text{ kg/m}^2 \text{ s}$

Fig. 1.3 Comparison of calculated and measured temperatures for the data of Janssen and Kervinen (1975)

a) $G \sim 675 \text{ kg/m}^2 \text{ s}$

b) $G \sim 1000 \text{ kg/m}^2 \text{ s}$

Fig. 1.4 Comparison of calculated and measured temperatures for the data of Swenson et al. (1965)

a) $G \sim 1360 \text{ kg/m}^2 \text{ s}$

b) $G \sim 950 \text{ kg/m}^2 \text{ s}$

available in a system, and therefore, may depend on the nucleation characteristics of the wall, amount of dissolved gas and foreign particles in the flow.

An attempt has been made during this quarter to include the effect of flow regime (or void fraction) in the formulation of the interfacial area density, A_s . For bubbly flow with equal-sized spherical bubbles at a cross-section, the interfacial area density can be written as

$$A_{s,\text{bubbly}} = \frac{3\alpha}{R} = 6 \left(\frac{\pi}{6} \right)^{1/3} N_b^{1/3} \alpha^{2/3} \quad (1.2)$$

where R is the bubble radius, N_b is the number of bubbles per unit volume and α is the void fraction of the mixture.

For the bubbly-slug flow, besides the slug (or the larger bubble), there exist other smaller bubbles in the continuous liquid phase. It is assumed that the slug is cylindrical in shape with two hemi-spherical caps at the ends. It is further assumed that on the average, the length of a slug is three times its diameter, and there is one such slug in three pipe diameters. The rest of the volume is filled with bubbly mixture of void fraction α_{bubble} . With this physical description of the bubbly-slug regime, the interfacial area density becomes

$$A_{s,\text{bubbly-slug}} = \frac{4.3268}{D} \left[\frac{\alpha - \alpha_{\text{bubble}}}{1 - \alpha_{\text{bubble}}} \right]^{2/3} + \frac{(1 - \alpha)}{(1 - \alpha_{\text{bubble}})} 6 \left(\frac{\pi}{6} \right)^{1/3} N_b^{1/3} \alpha_{\text{bubble}}^{2/3} \quad (1.3)$$

where D is the pipe diameter, and α is the void fraction of the mixture including the slug.

For the droplet-annular flow, a liquid film exists at the wall with equal-sized spherical liquid droplets at the core. Defining α_{core} to be the void fraction of the core region (including the droplets) only, and N_d to be the number of droplets per unit volume of the core region, one can express the interfacial area density as

$$A_{s,\text{drop-annular}} = \frac{4}{D} \sqrt{\frac{\alpha}{\alpha_{\text{core}}}} + 6 \left(\frac{\pi}{6} \right)^{1/3} N_d^{1/3} (1 - \alpha_{\text{core}})^{2/3} \left(\frac{\alpha}{\alpha_{\text{core}}} \right) \quad (1.4)$$

For dispersed droplet flow, the area density simply becomes

$$A_{s,drop} = \frac{3(1-\alpha)}{r} = 6 \left(\frac{\pi}{6}\right)^{1/3} N_d^{1/3} (1-\alpha)^{2/3} \quad (1.5)$$

where r is the average droplet radius.

Behavior of equations (1.2) through (1.5) are shown in Fig. (1.5) for various equal values of N_b and N_d . It is assumed that the bubbly-slug regime starts at a void fraction of 20 percent and α_{bubble} is equal to $(\alpha + 0.2)/2$. For the droplet-annular flow, the starting void fraction is taken as 70 percent, and the following relations are assumed for α_{core} :

$$(1 - \alpha_{core}) = \frac{1}{2} (\alpha - 0.7) \text{ for } 0.7 \leq \alpha \leq 0.85$$

and

(1.6)

$$(1 - \alpha_{core}) = \frac{1}{2} (1 - \alpha) \text{ for } 0.85 \leq \alpha \leq 1$$

The tube diameter is taken as 1 cm for the test case. For larger diameters, the area density corresponding to the intermediate values of void fraction will be smaller, as implied by equations (1.3) and (1.4). An examination of Fig. (1.5) reveals that the relationship between the interfacial area density and the void fraction, over the entire range, may be expressed by the following:

$$A_s = C_s [\alpha(1 - \alpha)]^{2/3} \quad (1.7)$$

where C_s is a dimensional constant (with dimension of one over length) which will probably depend on the nucleation characteristics and the size of the system. It is recognized that equations (1.3) and (1.4), and the associated assumptions are very crude. Nonetheless, these assumptions have resulted in a smooth relationship between the void fraction and the interfacial area density over the entire range of void fraction. Moreover, equation (1.7) exhibits correct dependence with void fraction at the two extremes, i.e., at $\alpha \rightarrow 0$ and $\alpha \rightarrow 1$.

Interfacial Heat Flux, $\dot{q}''_i$:

For high pressure, high speed critical flow situations, where the relative velocity between the phases is expected to be of secondary importance, heat is transferred from the liquid to the interface mainly by transient conduction. Asymptotic conduction-controlled bubble growth laws in a constant

Fig. 1.5 Expected Interfacial Area Densities with Void Fractions

liquid superheating condition, as developed by Plesset and Zwick (1954), and Forster and Zuber (1954, 1955), are well-known. The interfacial heat flux according to these models can be written as

$$\dot{q}_i'' = K_s \frac{k_\ell}{\sqrt{\pi a_\ell t}} (T_\ell - T_{\text{sat}}) \quad (1.8)$$

where K_s is a constant incorporating the effect of moving boundary with a value of either $\sqrt{3}$ (Plesset and Zwick) or $\pi/2$ (Forster and Zuber), k_ℓ is the thermal conductivity of the liquid, a_ℓ is the thermal diffusivity of the liquid, t is the time from bubble nucleation, T_ℓ is the liquid bulk temperature, and T_{sat} is the saturation temperature and also the vapor temperature. In terms of heat transfer coefficient, equation (1.8) can be cast into

$$h = \frac{\dot{q}_i''}{(T_\ell - T_{\text{sat}})} = K_s \frac{k_\ell}{\sqrt{\pi a_\ell t}} \quad (1.9)$$

Although equation (1.8) or (1.9) is valid strictly for a constant value of $(T_\ell - T_{\text{sat}})$, equation (1.9) is compared with the data obtained in variable superheat situations (See Fig. 1.6 and 1.7). In Fig. 1.6, it is compared with the steam-water data of Niino (1975), whereas in Fig. 1.7, it is compared with the nitrogen data of Hewitt and Parker (1968). In both figures, the experimental values of the instantaneous heat transfer coefficient are calculated from the measured values of the instantaneous bubble radius and the instantaneous liquid superheating, $(T_\ell - T_{\text{sat}})$, which was varying. It is surprising to see the good agreement between the trends of the data and equation (1.9), at least for shorter times ($t < 50$ msec). In case of a hypothetical LOCA, where the critical mixture velocity is on the order of 100 m/sec and the pipe length is on the order of 1 m, the residence time is on the order of 10 milliseconds. Therefore, it is expected that although the liquid superheating will vary, the form of equation (1.9) can still be used in determining the interfacial heat flux during a flashing flow.

The effect of relative velocity, which may be important at low pressures, may be incorporated in the interfacial heat flux term by modifying the expression of Aleksandrov (1967). The modification is necessary so that the expression would approach the steady-state value for large time. The modified expression is

Fig. 1.6 Comparison of Plesset and Zwick (1954) and Forster and Zuber (1954, 1955) heat transfer coefficient with measured instantaneous heat transfer coefficient during variable liquid superheating

Fig. 1.7 Comparison of Plesset and Zwick (1954) and Forster and Zuber (1954, 1955) heat transfer coefficient with measured instantaneous heat transfer coefficient during variable liquid superheating

$$\dot{q}_i'' = \frac{\sqrt{3} k_\ell (T_\ell - T_{\text{sat}})}{\sqrt{\pi} R} \frac{\left(\frac{2U_r R}{a_\ell}\right)^{1/2}}{\left(\frac{2U_r t}{R}\right)^{1/2}} \left[1 + \frac{2}{3} \frac{U_r t}{R}\right]^{1/2} \quad (1.10a)$$

$$= \frac{\sqrt{3} k_\ell (T_\ell - T_{\text{sat}})}{\sqrt{\pi a_\ell t}} \left[1 + \frac{2}{3} \frac{U_r t}{R}\right]^{1/2} \quad (1.10b)$$

where U_r is the relative velocity and R is the bubble radius. A comparison of the modified Aleksandrov, et al. equation with the original Aleksandrov (1967) equation and the Chao (1969) solution, which is valid only for a stationary boundary, is shown in Fig. 1.8.

From equations (1.1), (1.7), and (1.9), one can now write the expression for the volumetric vapor generation rate, where the effect of relative velocity is negligible, as

$$\Gamma_v = C_\Gamma \frac{k_\ell}{\sqrt{a_\ell t}} \frac{(T_\ell - T_{\text{sat}})}{\Delta i_{fg}} [\alpha(1 - \alpha)]^{2/3} \quad (1.11)$$

where C_Γ is the constant to be determined from experiments. If the effect of relative velocity is important, equation (1.10b) should be used for the interfacial heat flux term, and equation (1.11) is modified as

$$\Gamma_v = C_\Gamma \frac{k_\ell}{\sqrt{a_\ell t}} \frac{(T_\ell - T_{\text{sat}})}{\Delta i_{fg}} [\alpha(1 - \alpha)]^{2/3} \left[1 + \frac{2}{3} \frac{U_r t}{R}\right]^{1/2} \quad (1.12)$$

Note that C_Γ has a dimension of one over length, and originates from the constant C_s which appears in the expression for the interfacial area density, i.e., equation (1.7).

As a first step, equation (1.11) will be used with the data of M. Reocreux (1974) to evaluate C_Γ . Work has begun in this direction,

Fig. 1.8 Comparison of Various Heat Transfer Formulations for a Translating Bubble

1.2 Design, Construction, and Checkout of Experimental Equipment

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

All design drafting on the flow circuit including piping schematic, layout and details, has been completed. The interlock system has been tentatively established and requires only a check for compatibility with system controls of items on order (heaters for example) and in-house review to be finalized.

Costly and/or long lead items have been ordered or sent out for bid. Included are such items as heaters and pressurizers, control valves and flow meters. High temperature pump seals have arrived and await installation.

A design review was conducted early in this quarter, and only minor changes were required. Following this the loop modules were placed in their final positions and anchored to the floor. Connecting piping is currently being installed and water is being circulated in existing portions of the flow circuit to check out components.

Due to the high cost of purchased heaters, the suitability of converting a spare bank of heat exchangers to be used as heaters is currently being evaluated. This evaluation is expected to be completed approximately the same time bids are received from heater manufacturers, so that the most economic choice can be made.

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

Design layout of the test section including traversing of gamma densitometer has been completed. Detail drawings of the individual components are currently being prepared and an additional draftsman has been assigned to aid to this effort. Figure 1.9 shows the layout of the test section with associated traversing mechanisms.

The inflatable seal design, described in the last Quarterly, has suffered some serious set backs. First, silicone rubber samples, which were tested proved to be totally inadequate for use in our testing environment. Ethylene-propylene (EPDM) was chosen to replace the silicone, however no suitable adhesive could be found to adhere the EPDM to stainless steel at 350°F. The seal design was changed to provide mechanical captivation to eliminate the need for an adhesive. This design was sent out to several manufacturers, but none felt they could produce the seal and meet the required tolerances using EPDM. Presently, another seal design is being evaluated. This is a major design change since the need for inflating and deflating the seal has been eliminated. With this new design, such parameters as seal durometer and instrument probe rake cross section have to be carefully controlled to protect the fiber optic probes.

Several methods of manufacturing have been reviewed for fabricating the venturi portion of the test section. Among these were spinning, electro-deposition, rolling, casting, and machining from a solid block. Spinning was judged to be the most practical, and a local contractor is presently fabricating the venturi by this method.

Fig. 1.9 Test Section Layout

Testing continues to determine whether hydrodynamic flutter of the instrument probe rake will be a problem. A test loop has been constructed and debugged. Testing was conducted, and no visual vibration was noted. More sensitive instrumentation is currently being developed.

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

In order to obtain some preliminary data and get acquainted with the operation of Cadimium Telluride detectors, some experiments were conducted with the plexiglass-filled stainless steel tubes as described in the last Quarterly report (Jones 1977).

The γ -source, Americium 241, was around 200 millicuries and emitted γ -rays at an energy of 60 kev. The source-detector distance was 4 in. The Cadimium Telluride detector was connected to a preamplifier followed by a linear amplifier, a channel analyzer, a ratemeter, and a scaler.

The results obtained, as expected, showed a linear variation of the ratio of count rates with void fraction when plotted on a semi-logarithmic scale.

Regarding the final γ -ray absorbtion system to be used in the main loop, the thulium sources were ordered as 0.1" x 0.1" x 1" metal strips. The casings will be machined in the BNL shops, and the source irradiated and rendered active in the BNL reactor. This will eliminate the buying of new sources after their decay (129 days half life), which seems to be expensive. The final design of the source holders, collimators, the detector holders, amplifying circuits, and traversing mechanism are almost complete.

(d) Facilities (W. J. Leonhardt, J. H. Klein)

Construction of the instrumentation storage room, started last quarter, has been completed. In addition to storage, design and repair of instruments, the room presently houses the data acquisition system used by this facility.

Conversion of the old generator room to a control room has begun. When completed, this room will house not only the flow circuit controls, but the above mentioned data acquisition system as well. A cooling tower has been purchased and awaits delivery to be installed on the roof of the control room, which is adjacent to the flow circuit.

Funds and authorization have been obtained to construct a 450 square foot storage facility for storing experimental equipment, which is infrequently used. Bids are currently being solicited for construction of this facility.

1.3 Development of Instrumentation Techniques

1.3.1 Analytical Modeling (N. Abuaf)

If a signal is fed to several recording devices, simultaneously, and assuming that there is no distortion of the input or noise, the outputs of each one will be similar in nature to the original input, but may be shifted by a certain time lag introduced by the recording equipment, τ_d .

By using the covariance and the cross correlation, an expression can be derived which will allow the determination of the time lag introduced by each recording device.

Assume a sine input function $f(t) = A \sin \omega t$

Output of the first recording device $f_1(t) = A \sin \omega t$

Output of the second recording device $f_2(t) = A \sin \omega(t + \tau)$

The covariance function (the auto correlation function) at lag τ of a single function is:

$$C(\tau) = \lim_{T \rightarrow \infty} \frac{1}{T} \int_{-T/2}^{T/2} X(t) X(t + \tau) dt \quad (1.13)$$

Applying this definition to the sine function,

$$C(\tau) = \lim_{T \rightarrow \infty} \frac{1}{T} \int_{-T/2}^{T/2} A^2 \cdot \sin \omega t \cdot \sin \omega(t + \tau) dt \quad (1.14)$$

Performing the integration yields:

$$C(\tau) = \frac{A^2}{2} \cdot \cos \omega \tau \quad (1.15)$$

The variance of the sine wave is;

$$C(0) = \frac{A^2}{2} \quad (1.16)$$

and one obtains that:

$$\frac{C(\tau)}{C(0)} = \cos \omega \tau \quad (1.17)$$

which is unity for $\omega\tau = 2n\pi$, where both devices behave identically.

If another recording device introduces a phase angle, $\phi = \omega\tau_\ell$, where τ_ℓ is the time lag, such that $f_3(t) = A \sin \omega(t + \tau + \tau_\ell)$, the cross correlation function can be used between input and output to derive an expression for the time lag τ_ℓ .

$$C(\tau_\ell) = \lim_{T \rightarrow \infty} \frac{1}{T} \int_{-T/2}^{T/2} A^2 \cdot \sin \omega t \cdot \sin \omega(t + \tau + \tau_\ell) \cdot dt \quad (1.18)$$

The integration of this expression yields

$$C(\tau_\ell) = \frac{A^2}{2} \cos \omega(\tau + \tau_\ell) \quad (1.19)$$

and dividing it by the variance of the input function ($f = A \sin \omega t$);

$$\frac{C(\tau_\ell)}{C(0)} = \cos \omega(\tau + \tau_\ell) \quad (1.20)$$

which has the value of unity, $\cos \omega(\tau + \tau_\ell) = 1$ for

$$\omega(\tau + \tau_\ell) = 2n\pi \quad (1.21)$$

Equations (1.17) and (1.21) allow the determination of the time lag once the ratio of the covariance to the variance or the cross correlation to the variance of the input is known.

1.3.2 Design, Construction, and Checkout of Experimental Equipment

(a) Local Instruments (N. Abuaf, A. Swoboda)

The construction techniques for the optical probes have been developed and applied successfully in the building of probes that can operate at room temperatures in water-air mixtures. Concerning the final probe which should operate at 350°F and 150 psia in a steam-water environment, different materials have been tested, and the search for the most optimum construction techniques and choice of materials is still being pursued.

The construction procedure is documented in Fig. 1.10. Two 0.002" or 0.005" fibers (Dyonics, or Galileo) are inserted into a 0.010" or 0.020" OD stainless steel

tube (Superior Tube). One end of the two fibers is fused together by means of a minitorch, forming a spherical bead. This fused end of the fibers is pulled into the stainless steel tube and epoxied. The other end of the two fibers is separated, and encased into two pieces of stainless steel tubing (0.010" OD), respectively. The next step consists of epoxying the ends of the fibers and the bifurcation region. The fused end of the fibers is ground and polished at a 45° angle to the axis of one of the fibers. The tip is rotated 180° and the other side of the tip is ground and polished in a similar way at 45° to the axis of the other fiber. Hence, the finished tip has an included angle of 90° (See Fig. 1.10).

After grinding and polishing the free ends of the two fibers flat, one of them is placed in front of a light source (incandescent light, 3V) and the other in front of a Hewlett Packard PIN photodiode (5082-4024). An amplifier as shown in Fig. 1.11, with a rise time of 20 μ sec is used to enhance the output before going into the oscilloscope (Hewlett Packard Application Note 915).

The output of the probe in water, air and its response to the passage of bubbles was recorded and is shown in Fig. 1.12. These results are qualitative in nature and are presented here to show the successful operation of the optical probe in an air-water system.

Several similar probes have been built, and development of an amplifier with a faster response time ($< 1\mu$ sec) is being pursued.

Concerning the materials needed to build a probe that can withstand 350°F and 150 psia, in a steam-water environment, several tests were conducted in an autoclave.

Ten different sample fibers (Galileo, Corning, Math Assoc., Dyonics, Incom, Wilt Lab, Dolan and Jenner, Valtec) were checked in water and steam under the above mentioned conditions for periods ranging from one to three days. Most of the fibers which are clean and transparent before the test, became white foggy and opaque after being subjected to the test conditions. In addition to that, they became very brittle, wiry and broke into pieces or powder. It seems that the coefficient of expansion and contraction of the core and of the cladding are different, thus causing the fiber to disintegrate when subject to the test conditions. The fibers before and after testing were taped on microscope slides and the information described here will be documented with macrophotography.

The CORNING fibers did stand the test conditions, but only a 0.005" diameter fiber can be obtained. The clear fused quartz fibers of WILT Laboratories also stand the test conditions, but they are very difficult to fuse together, are less flexible, and have no cladding at all which may present substantial light losses to the surroundings. The Galileo G-1000 fibers (0.005") can be manufactured as 0.002" fibers but are marginal and show a similar deterioration after a few days of testing.

Different epoxies (Epoxy Technology Inc., Elmer, Summers Labs Inc.) were also tested in the autoclave under similar steam-water conditions. The EPO-TEK 330 of the Epoxy Technology Inc. seemed to stand the test conditions.

STEP 1: INSERT AND WELD OPTICAL WAVEGUIDES

STEP 2: SEPARATE ENDS - EPOXY TIP AND BIFURCATION

STEP 3: POLISH TIP TO 45° ANGLE

Additional epoxies and fibers are being sought from different manufacturers and once obtained will be subjected to the test conditions in order to determine the optimum materials for the building of the final probes.

Checkout of the Optical Probes:

Regarding the calibration of the optical probes by means of the bubble generator described in the second Quarterly Progress Report (Jones 1977), some preliminary photographs of the bubbles were recorded by means of a stroboscopic illumination. The bubble generator did not operate as efficiently as expected. Several problems, the distortion of the bubbles, helical rising paths, and slow rise velocities which caused the bubble to escape the probe, were encountered. This set up helped us record the probe response presented in Fig. 1.12.

The modified set-up shown in Fig. 1.13 consists of a glass tube, 1/4" ID, and 32.5 inches long. Air can be injected at the base through a needle valve. The bubbles or slugs of air now fill the entire tube cross-section eliminating some of the disadvantages of the previous design. In order to obtain high bubble velocities, the water in a reservoir downstream from the bubble can be pressurized by means of bottled air. Opening the ball valve releases the pressurized water accelerating the liquid and the air bubble at the same time. The velocity of the bubbles along the glass tube are measured before reaching the probe tip by means of two light sources and two detectors, sensing the passage of the bubble and triggering and stopping a counter. This gives the time for the bubble to travel between the two points. Knowing the distance between them, one can calculate the average bubble velocity. Fig. 1.14 presents some results of the bubble velocity as a function of the pressure applied in the storage tank. As observed, the bubble velocities ranged from 0.25 in/sec to 60 in/sec for a pressure range of 0 to 5 psi above atmospheric pressure. This velocity range is compatible with the time response of the optical probe amplifier circuit. The tank and set-up could stand much higher pressures, therefore, higher bubble velocities could also be obtained with the same set-up.

The output of the detectors also contains the information about the length of the bubbles once their average velocities are known. After passing through the second detection station, the bubble passes through the optical probe. The output of the optical probe at the passage of the interface and the bubble are also recorded simultaneously. In the experiments conducted with the above described apparatus, two main parameters, the bubble penetration time and the amplitude of the signal at the photodiode were recorded as a function of the bubble velocity.

In Fig. 1.15, the amplitude of the signal (in mv) is observed to decrease with the bubble velocity, as the latter was increased from 0.6 cm/sec to 130 cm/sec. Fig. 1.16 presents the variation of the bubble penetration time as a function of the bubble velocity. The bubble penetration time is defined as the time period between the arrival of the bubble at the tip and the moment when the steady-state signal value is reached for the first time. In Fig. 1.16, a plot of $t(m \text{ sec}) = C/V(\text{cm/s})$ is also presented for comparison.

Both of these variations are new and not observed or reported before. Therefore, a more detailed investigation of the probe response characteristics is necessary and is being conducted.

Fig. 1.11 Schematic Representation of the Optical Probe

Fig. 1.12 Typical Output of the Optical Probe

Fig. 1.13 Experimental Set-Up for Probe Calibration

Fig. 1.14 Bubble Velocity vs Tank Pressure

Fig. 1.15 Probe Signal Amplitude
vs Bubble Velocity

Fig. 1.16 Bubble Penetration
Time vs Bubble Velocity

(b) Signal Analysis System (G. A. Zimmer, N. Abuaf)

The crossbar scanner interface was received and installed in the computer. Verification tests were performed satisfactorily and the device was accepted. A driver was written for the scanner and is operational. Several programs have been written for fast reactor experiments which acquire data using the scanner and high resolution voltmeter, reduce the data to engineering units and display the reduced data in graphical and tabular form. The oscilloscope vector display driver and character generator have been completed and are operational. A test program was written and executed with a non-storage type oscilloscope successfully, but performance with a non-storage type device is not satisfactory. The non-storage type display requires refreshing every 20 milliseconds and for a typical display, without much complexity, the computer spends 75 percent of its time maintaining the display. A storage type device will be acquired.

The Hewlett Packard 740B voltage standard was received and accepted. The standard has a span of one microvolt to 1000 volts DC in four ranges, with one microvolt resolution in the zero to one volt range.

Delivery of the multiplexed ADC subsystem, reference junction and quartz pressure standard has slipped. Delivery of these items is now expected in mid-July.

A computer program performing the Fast Fourier Transform of a digitally sampled function was also rendered operational. Additional subroutines giving the real and imaginary parts of the transform, and the power spectra are also available. These subroutines were checked by means of known analytical functions and also functions generated by a function generator (sine wave, square wave, saw tooth etc.). The program was also used in the Fourier Analysis of the signals obtained from the passage of bubbles through two stations of light sources and detectors. Development and improvement of this system is still in progress.

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2. THOR CODE DEVELOPMENT

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

2.1 General Program Objectives and Approach

The THOR Code Development Program is designed to produce an advanced computer code for the prediction of accident-induced thermo-hydraulic transients in water-cooled nuclear reactors.

The primary objectives are:

1. The model should include the effects of thermal non-equilibrium, as well as of non-equal velocities between the vapor and liquid phases.
2. The reactor components should be modeled to include the specific characteristics of each component.
3. The axial variation of the transient fission power must be modeled.
4. The program should include the capability for an initial steady state computation.
5. The code structure should be modular, permitting its application to different system configurations, as well as providing for ready improvement of individual components and process subroutines.
6. The code should be executing rapidly, for instance, for frequent application in parametric studies.

To achieve the above objectives a lumped parameter approach has been formulated for the various component models and an explicit solution scheme is being followed.

2.2 Status and Current Goals

A considerable portion of the work directed towards the above objectives was done and has been reported in previous quarterly reports. This includes, in particular, the formulation of lumped parameter models of components with non-homogeneous non-equilibrium flow, a system steady state formulation and demonstration, as well as process models for drift flux relationships and choking conditions. Modeling of 1-dimensional neutron kinetics including the axial variation of fission power is currently in progress.

To demonstrate the feasibility, the accuracy, and the computing speed potential of the explicit lumped parameter scheme for system simulations, the following demonstration test cases were set up:

1. Several simple demonstration test cases, which have been formulated and evaluated.

2. A single loop PWR blowdown model is being prepared for demonstration.

Upon completion of the above demonstrations, a hard wired version of a full two-loop PWR code including blowdown and reflood will be developed. Modularity will be implemented later.

2.3 Current Achievements

2.3.1 Simple Demonstration Test Cases

These test cases consisted of two categories:

1. Pipes connected in series and in parallel.
2. Loops of plena with heated and cooled pipes.

Since the primary objective of these test cases was to demonstrate the concept and the coupling of lumped components, several simplifications in the thermo-hydraulics were made. In particular, homogeneous flow and thermal equilibrium were generally assumed.

2.3.1.1 Pipe Test Cases (G.S. Srikantiah, U.S. Rohatgi, S. Lekach, A. Dubow, R. Krasny)

In this series of test cases, lumped parameter formulations of pipes connected in series with and without area change as well as pipes connected in parallel by means of tee junctions have been developed, programmed and tested for different transient flow conditions. The assumptions made in this formulation are:

- (i) saturation properties evaluated using volume averaged values of pressure
- (ii) local vapor mass fraction is replaced by the volume averaged value of c except in inertia terms
- (iii) linear spatial variations for vapor concentration and pressure in the lumped parameter pipes.

The lumped parameter description of a single pipe has been given in detail in the last quarterly report. The description of area changes and tee junctions follows that of Ginzburg (1958) and Idel'chik (1960). The ordinary differential equations and algebraic equations which describe transient flow in the pipes together with the algebraic equations which describe the flow in area changes and tees have been combined and reduced into a simultaneous set suitable for explicit integration. This process includes differentiation of some of the algebraic equations.

The solution procedure consists of solving the algebraic equations by use of the nonlinear equation solver ZSYSTEM and integration of the ODE's by use of the IMS Library routine DVOGER.

Several transient test cases were programmed and the results compared with the distributed parameter computations. The boundary conditions applied are:

- (i) constant inlet conditions, exponentially decaying outlet pressure,
- (ii) inlet and outlet pressures decaying exponentially,
- (iii) constant inlet conditions with sinusoidally varying outlet pressure.

The details and results of the computations for the coupling of two lumped pipes in series and for two pipes in series with an area contraction in-between were described in the last quarterly report. The geometry and interconnections for the three pipe network are shown in Figure 2.3.1. The lumped parameter and distributed parameter computations were performed for the boundary conditions described above. The distributed parameter calculations were done by using a first order Euler-implicit formulation in which six nodes were used in each pipe and the tee-branches were represented by linearized algebraic equations.

The results of these computations are shown in Figures 2.3.2 - 2.3.6. In Figure 2.3.2 are shown the results of lumped and distributed calculations for constant inlet conditions ($P_{in} = 100$ bar, $h_{in} = 1.41267$ MJ/Kg) and exponentially decaying outlet pressure ($P_{out} = 87.5 + 9 \exp(-t/6)$ bar). Figure 2.3.3 shows a transient calculation in which both the inlet and outlet pressures decayed exponentially ($P_{in} = 91 + 9 \exp(-t/12)$, $P_{out} = 87.5 + 9 \exp(-t/6)$ bar). The spatial profiles for the mixture mass flux G and vapor concentration c in pipe A for the second case are shown in Figure 2.3.4.a and 2.3.4.b at steady state and at 5 sec. after start of transient. Figures 2.3.5.a and 2.3.5.b show the comparison of the lumped and distributed parameter computations for a constant inlet pressure (100 bar) and a sinusoidally varying outlet pressure ($P_{out} = 95.5 + 1.5 \sin(\pi/2 + t)$ bar). Finally, in Figure 2.3.6 is shown a transient in which the inlet pressure is constant at 100 bar and the outlet pressure is varied sinusoidally with a frequency faster than the transit time of the fluid through the pipes. In all these cases the agreement between the lumped and distributed cases is excellent. The maximum deviation between the lumped and the distributed results in the entire series is less than 5%. The comparative computing time requirements for the entire pipe series of calculations is shown in Table 2.3.1. The lumped parameter cases are 5-20 times faster than the corresponding distributed computations.

2.3.1.2 Loop Test Cases (P.G. Kroeger, J.H. Jo)

The configurations of loop test cases to be investigated is shown in Figure 2.3.7. The results for the natural convection cases obtained with configuration 1 were described in the last quarterly report.

The thermo-hydraulics for these configurations were modelled assuming homogeneous flow and thermal equilibrium. The dissipation terms in the energy equation were neglected. Complete mixing in both plena was assumed. The fluid was in the two-phase region in all cases at all times.

A distributed solution was obtained, using a first order method of characteristics in the pipes and a first order Euler explicit integration in the plena.

The lumped model included the following further assumptions:

1. The density was considered to be a function of enthalpy and a reference pressure. The pressure derivative of the density is neglected, i.e.,

$$\frac{1}{\rho} = \frac{1}{\rho_f(p_o)} + \frac{h - h_f(p_o)}{\rho_{fg}(p_o) h_{fg}(p_o)} ; \left(\frac{\partial \rho}{\partial p} \right)_h = 0$$

where

$$\frac{1}{\rho_{fg}} = v_{fg} = \frac{\rho_f - \rho_g}{\rho_f \rho_g}$$

2. The pressure terms in the thermal energy equation were neglected.
3. A linear enthalpy profile was assumed for each pipe lump, corresponding to the steady state solution with constant axial heat input.

Table 2.2.2 summarizes the design and operating data used for the forced flow cases of configurations 2 and 3. While FC-1 represents a simplified initial geometry, FC-2 uses system volumes and heating and cooling rates representative of actual PWRs (Shearon Harris Nuclear Power Plant).

All transients were initiated by changing heating/cooling rates, pump head or blowdown rate over a time interval $\Delta t_{rise} = 1$ sec, using an S-ramp. Figure 2.3.8 shows the results for a simultaneous trip of heating and cooling rates from its original values of 640MW/m to zero. The mass flow and enthalpy leaving the heated pipe (core) are shown. The agreement of the lumped solution with the distributed solution is very good. The peaks are slightly less pronounced in the lumped case. ("Lumped 1" presents a solution using a linearized density function as it was used in the previous natural convection cases.)

Figure 2.3.9 shows the results for a simultaneous heating/cooling and pump trip, where the mass flow decays to zero. The agreement between the lumped and distributed solutions is again very good. As noted in this figure, the distributed 20 node and 40 node solutions virtually coincided, which was the case for all the forced flow runs. Results for a cooling trip only are shown in Figure 2.3.10, using the data corresponding to our actual PWR configuration. The agreement between lumped and distributed solutions is particularly good here.

A run with initiation of blowdown from the lower plenum to obtain flow reversal in the core is shown in Figure 2.3.11. The lumped mass fluxes again agree well with the distributed solution, as shown here for the core entrance mass flux, which goes through a flow reversal. While the core exit enthalpies (h_4) also agree well, the core entrance enthalpy shows a temporary deviation from the distributed solution, at the time of flow reversal. This discrepancy is due to the simplifying assumption of a linear enthalpy profile, which is

clearly not satisfactory at this time. To obtain a more accurate solution here one would have to use a more appropriate profile function during the flow reversal.

To extend the results to higher mass flow rates and larger pressure differences a blowdown run from a system with larger plena and higher blowdown rates was made. The results are shown in Figure 2.3.12 and 2.3.13. In these cases the model "Lumped 1" evaluated all pipe densities at constant upper plenum reference pressure, while in "Lumped 2" the pipe average pressures were used as reference pressures for the densities. In particular, the "Lumped 2" solution agrees closely in mass fluxes and lower plenum pressure with the distributed solution. The discrepancy between the enthalpies of the lumped and distributed solutions is apparently due to the neglect of the pressure terms in the thermal energy equation at a time where the heating and cooling rates have gone to zero, i.e., at a point where this simplifying assumption is not warranted.

Based on some of the semi-scale experiments, we expect core and steam generator Mach numbers not to exceed 0.3 during blowdown. However, to establish the accuracy of the lumped model at higher flow rates, steady state results with reduced friction factors are presented in Figures 2.3.14. The results show that even at a Mach number of 0.6 the error of the lumped mass flux amounts to only 10% while it is 2.7% at $Ma = 0.33$. The enthalpies agreed well in all cases. (The "Lumped 2" enthalpies were similar to the "Lumped 1" enthalpies and were therefore not shown separately here. "Lumped 2" again uses the pipe average pressures for the density computation, while "Lumped 1" uses the upper plenum pressure).

In summary, the lumped loop demonstration test cases have not presented any coupling problems or numerical instabilities. The lumped results compared well with the distributed results in particular at the flow rates expected during blowdown.

2.3.1.3 Conclusions

The simple demonstration test cases have been completed.

The following conclusions can be drawn:

1. The coupling of lumped components did not present any difficulties in the loops of plena and pipes.
2. In the pipe cases successful component coupling required differentiation of some of the algebraic equations and neglecting of higher order derivatives which were generally small in these cases.
3. The lumped results compared generally well with the distributed results as regards accuracy.
4. The lumped programs typically executed three to ten times faster than the corresponding distributed programs (neither program was optimized for run times).

2.3.2 Single Loop PWR Blowdown

2.3.2.1 System balance equations (D.I. Garber, A. Dubow, R. Krasny, G.S. Srikantiah)

The implementation of the "hard-wired" single loop blowdown demonstration case demands a routine (DERIV) which is called by the O.D.E. solver to provide the derivatives of the ordinary differential equations being integrated. This routine must first solve a system of non-linear algebraic equations for interfacial values of p, h, c, G and the derivatives of $\langle G \rangle$. In the present case there are 70 such unknowns and equations. Once the above values have been determined the remaining derivatives in the problem can then be computed.

The coupling equations which form a part of the above algebraic equation set constitute the mass flux continuity at those six stationary interfaces between components which have temporal inertia. These have been derived from the volume integrated mass balance equations corresponding to components on each side of the interface. These equations, along with the volume integrated momentum balances and the interfacial (nodal) enthalpy and vapor mass concentration equations form the above set of seventy non-linear algebraic system balance equations which have to be solved at each time step.

A method of solution has been developed which solves the 70 equations by a decomposition method, solving sub-systems of equations no larger than 17 by 17. The decomposition technique increases speed of computation per pass by a factor of 25 with an associated reduction in working storage. A separate routine to provide those coefficients, properties, etc. that appear in the above algebraic equations has been written in a form compatible with the decomposition technique and can be called either before and/or during the algebraic solution. During this procedure estimated values of the variables outside of the block are used in finding a solution for this block and the estimated set will be updated after the corresponding block has been solved.

A dummy set of equations having the identical structure matrix but with known solutions have been generated and utilized to familiarize with and check the code.

2.3.2.2 System Ordinary Differential Equations (P.G. Kroeger, J.H. Jo, D.I. Garber)

The ordinary differential equations solver DVOGER, maintained by the International Mathematical and Statistical Libraries, Inc., was selected for use in the single loop blowdown code. This routine is a modification of the well known stiff equation solver DIFSUB by C.W. Gear (1971) and offers the use of a stiff equation solver as well as an Adams predictor-corrector for non-stiff cases.

Using two of the loop demonstration test cases (Section 2.3.1.2) a range of the appropriate convergence and accuracy control parameters for initial use in the single loop blowdown code were established.

The subroutine DERIV calls the component routines for the derivatives of the ordinary differential equations subsequent to the solution of the above algebraic equations. Its overall structure has been determined and the following component subroutines have been completed using the homogeneous equilibrium model:

- single-region core
- single-region steam generator
- pumps
- adiabatic pipe
- plena

For the single loop blowdown demonstration only the pressurizer routine remains to be completed. At this time a set of the above plena and pipe routines will be substituted to represent a simplified model of a pressurizer with a surge line.

2.3.2.3 Coupling of Distributed Parameter Pipe With Lumped Parameter Pipe (S.V. Lekach, U.S. Rohatgi)

A method has been developed and tested for the coupling of distributed with lumped parameter pipe. For this purpose the lumped parameter-pipe-model has been modified into a modular format such that it can be called from the distributed parameter component routine. Drift flux terms have been included in the lumped pipe model. Further, generalized profiles for enthalpy and vapor fraction have been introduced in the lumped pipe model to account for four different flow situations, namely, through flow in either direction; filling and emptying. Flow direction-dependent multipliers have been used in these generalized profiles such that enthalpy and vapor mass fraction go through smooth transitions during flow reversal situations. These features have been successfully implemented and tested.

Coupling between the lumped pipe described above and a distributed pipe has been achieved by first expressing the end point values of the solution vector of the hydraulic variables of the distributed pipe as linear combinations of the end point values of the corresponding variables obtained by time-advancement of the lumped pipe. A forward elimination process in the distributed pipe produces a set of linear algebraic equations. These two sets of linear algebraic equations together with the interface equations at the junction and the specified system boundary conditions are solved simultaneously. The time step is completed by back-solving for the remaining variables. This approach has been applied to the case of a distributed pipe downstream of a lumped pipe. The lumped pipe integrations were performed using the R.K.S. routine. This method will be extended to different flow situations. Further, the IMS Library routine DVOGER will be used to integrate the lumped pipe ODE's in subsequent computations.

The second part of this work is concerned with the development of a generalized coupling method which can be applied to components integrated with different time steps. The components being coupled may be discrete or lumped or both. The technique consists of integrating the component with the lowest time step by taking into account the effect of the rest of the system components by means of a set of forward-eliminated equations. This procedure has been implemented in the discrete algorithm computer program and is being tested out.

2.3.2.4 Test Case Selection (U.S. Rohatgi, G.S. Srikantiah)

A literature search has been made to identify a suitable semi-scale experiment which can be used for simulation study by applying the single loop blowdown computer program. The semi-scale series 800 tests appear most suitable for this purpose, Heiselmann, M.W., et al. (1970), Olson, D.J. and Whitbeck, J.F. (1970). Though these series of tests are not as well documented as the more recent one and half loop tests, we have been able to construct a single loop with a core, a pressurizer, a heat exchanger, a pump, a tee junction and six sections of pipe with allowance for a break to simulate this type of test loop. The "hardwired" single loop program presently being developed with the above mentioned components will be exercised using this as a test case.

2.3.3 Component Modelling for Two-Loop PWR.

2.3.3.1 Downcomer (U.S. Rohatgi)

The previous two-regime downcomer model including a moving two-dimensional interface between the regions has been deferred temporarily since difficulties, in particular with the interfacial shear boundary conditions, made a timely implementation questionable.

A simpler semi-empirical model is being developed first. In this approach the downcomer is represented by a combination of a storage free tee and a diabatic pipe with mass, momentum and energy storage. Initially this diabatic pipe will be replaced by the available adiabatic lumped parameter pipe with drift flux terms and flow reversal features. The drift flux velocity will be computed from Wallis's flooding correlation. The appropriate expression for it has already been derived.

During the blowdown and subsequent refill stages, the tee goes through three different converging and diverging flow configurations while the lumped pipe can have four possible flow configurations. The logic to connect the tee and the pipe for these various flow configurations has been formulated and will be programmed.

2.3.3.2 Reactor Core Modeling (C.J. Ruger, J.M. Kaufman, T. Wang, S. Stein)

The motion of the flow regime interfaces was originally modeled in terms of kinematic waves. Recent experience indicates that it is substantially more advantageous to model the flow regime interfaces as kinematic shocks, thus eliminating the need for drift velocity gradients at the boundaries, which are

presently not known. The kinematic shock conditions for non-homogeneous interfaces in the core are presently being developed, for subsequent testing in a two region non-homogeneous thermal-equilibrium model with one moving interface.

In the kinematic shock conditions the pressure terms in the momentum jump equation are assumed to be dominant, resulting in equal pressures across the shock. Therefore, only the mixture mass and energy jump conditions need be considered. These jump conditions are coupled with the volume averaged momentum and energy equations and an algebraic expression for mixture mass flow, obtained by combining and integrating the original mixture mass and energy equations. The enthalpy profile relations and the interface specification that the enthalpy just below the interface is at saturation together with three boundary conditions, namely, both end pressures and entrance enthalpy for upward flow, completes the problem formulation. The simultaneous solution of these equations yields the interfacial velocity in addition to the time derivatives and terminal values of mixture enthalpy and mass flux.

The model has been formulated. We are presently investigating the best method of solution.

To further advance the full core model, while development of the kinematic shock interface treatment is in progress, the 6-region core model has been executed for the case of homogeneous equilibrium flow. In this case there are no interfacial discontinuities rendering the shock and wave interface models identical. With all interface motions suppressed the transient ran without problems. Approximately 12.5 sec real time took 5 sec of computing time. Convergence problems are apparently being caused by the liquid-to-bubbly interface algorithm. Suppressing only this interface motion, but letting the other four interfaces move, the program ran successfully, however, numerical stiffness was encountered.

A program to track reappearances and disappearances of regime boundaries has been written. For the case of a reappearing flow regime where the axial length, L_i , of the regime is initially zero, the evaluation of the derivatives for vapor mass concentration and mixture enthalpy

$$\lim_{L_i \rightarrow 0} \langle \bar{C} \rangle_i^0 \quad \text{and} \quad \lim_{L_i \rightarrow 0} \langle \bar{h} \rangle_i^0$$

becomes necessary. These limits and the limits of the profile functions have been evaluated from the closed form solution to the transient vapor and mixture mass conservation equations and have been included in the program. A test case for debugging the above program is being written.

The procedure for coupling the hydraulics with the fuel element heat transfer routines is being debugged. A routine to solve the initial steady state heat conduction problem in the core has been added to the transient fuel element heat transfer routines and is also being debugged.

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THREE LUMPED PIPES IN PARALLEL, CONNECTED BY FOUR TEES

$P_7(t)$, $h_7(t)$ --- PRESCRIBED

FIND G_i , P_i , c_i ; $i = 1 - 6, 9, 10$

$\langle G_A \rangle$, $\langle G_B \rangle$, $\langle G_C \rangle$, $\langle c_A \rangle$, $\langle c_B \rangle$, $\langle c_i \rangle$

Figure 2.3.1: Three Pipe Network Configuration

Figure 2.3.2: Comparison of Lumped and Distributed Parameter Computations for Exponentially Decaying Outlet Pressure Transient

Figure 2.3.3: Comparison of Lumped and Distributed Parameter Computation for Exponentially Decaying Inlet and Outlet Pressure Transient

Figure 2.3.4(a): Spatial Distribution of Mixture Mass Flux in Pipe A for Both Inlet and Outlet Pressures Decaying Exponentially

Figure 2.3.4(b): Spatial Distribution of Vapor Mass Concentration in Pipe A for Both Inlet and Outlet Pressure Decaying Exponentially

(a)

(b)

Figure 2.3.5(a), (b): Comparison of Lumped and Distributed Parameter Computation for Sinusoidal Variation of Outlet Pressure with a Period of 6 sec.

Figure 2.3.6: Comparison of Lumped and Distributed Parameter Computation for Sinusoidal Variation of Outlet Pressure with a Period of 1.5 sec.

TABLE 2.3.1

COMPARISON OF COMPUTING TIME

DEMONSTRATION TEST CASES II: LUMPED PIPE SYSTEMS

REAL/CPU TIME

TEST CASE		LUMPED	DISCRETE
1.	TWO PIPES IN SERIES	200	10
2.	TWO PIPES WITH AREA CHANGE	50	7.5
3.	THREE PIPES IN PARALLEL	20	4.4

COMPUTING SPEED RATIO L/D

1.	TWO PIPES IN SERIES	20
2.	TWO PIPES IN SERIES WITH AREA CHANGE	7
3.	THREE PIPES IN PARALLEL	4.5

NOTE: ALL DATA BASED ON TEST PROBLEMS ONLY.

NO OPTIMIZATION HAS BEEN MADE EITHER
ON L OR D COMPUTATIONS.

	FC-1	FC-2	FC-3	FC-4
Volumes: $V_{do} (m^3)$	77	77	5000	77
$V_b (m^3)$	42	42	5000	42
Lengths: $L_a = L_c (m)$	4.35	4.35	4.35	4.35
Areas: $A_a (m^2)$	10.	17.97	10	10
$A_c (m^2)$	10.	4.06	10	10
Gravity: $g_a (m/sec^2)$	0.	-10	-10	-10
$g_c (m/sec^2)$	0.	10.	10	+10
Heating/Cooling:	-640	-640	-640	-640
$Q_a (MW/m)$				
$Q_c (MW/m)$	+640	+640	+640	+640
Friction: $\frac{f}{d_h} \frac{1}{m}$	1.24 & 4.96	1.24	1.25	.1, .05, .025
Pumphead: $\Delta p_{pump} (bar)$	2.0	2.0	2.0	2
Upper Plenum Pressure:				
$p_d (bar)$	70	70	70	70
Initial System Enthalpy:				
$h_o (MJ/kg)$	1.5	1.5	1.5	1.5
$\Delta t_{rise} (sec)$	1.	1.	1.	1.
Blowdown Coefficient	0	0 & 0.0113	9.24	0
$(fA) = \frac{W_{ex}}{\sqrt{\rho \Delta p}} (m^2)$				

Table 2.3.2: Summary of Design and Operating Data for Forced Flow Runs

Figure 2.3.7: Loop Configurations

Figure 2.3.8: Core Exit Mass Flux and Enthalpy for Forced Flow Heating and Cooling Trip (Data FC-1)

Figure 2.3.9: Steam Generator Exit Mass Flux and Enthalpy for Forced Flow Heating and Cooling and Pump Trip (Data FC-1; Linearized Density Function)

Figure 2.3.10: Mass Flux and Enthalpy Variation During a Forced Flow Cooling Trip (Data FC-2)

Figure 2.3.11: Forced Flow Heating and Cooling and Pump Trip with Blowdown (Data FC-2)

Figure 2.3.12: Mass Fluxes and Lower Plenum Pressure during Large Plena Blowdown with Heating, Cooling and Pump Trip (Data FC-3)

Figure 2.3.13: Enthalpies During Large Plena Blowdown with Heating, Cooling and Pump Trip (Data FC-3)

Figure 2.3.14: High Velocity Steady State Flow (Data FC-4)