Liquid Metal Engineering Center (LMEC) Semiannual Technical Progress Report

July-December 1969

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INTRODUCTION

The overall objective of the LMEC is to provide optimum operation and management of the facility for implementing the following major functions:

- 1) Operation, maintenance, and modification of facilities designed to test and evaluate liquid metal components, and the performance evaluation of components tested.
- 2) Construction of authorized test and evaluation facilities.
- 3) Provision of training for industrial personnel in liquid metal system and component technology.
- 4) Operation of the Liquid Metals Information Center (LMIC).
- 5) Provision of engineering advice, consultation, and technical contract management to the U. S. Atomic Energy Commission (AEC) and its contractors, generally in the technical area of liquid metal component technology, with specific tasks assigned by the AEC Division of Reactor Development and Technology (RDT).
- 6) Preparation of RDT LMFBR-oriented tentative standards, and review of such standards prepared by others.
- 7) Development of a Quality Assurance program in accordance with RDT policy, implementation of this policy within LMEC, and support to RDT in implementing this policy, as directed, in RDT programs.

Highlights of LMEC achievements during CY 1969 include the following:

- 1) Initiated use of RDT standards by industrial contractors.
- 2) Drafted sodium pump standard for use in FFTF procurement package.
- 3) Provided IHX standard for use in FFTF procurement package.
- 4) Developed and/or assisted in issuance of 33 RDT Material and Process Standards for FFTF.
- 5) Initiated application of RDT standards to SCTI, SCTL, and SPTF.
- 6) Initiated LCTL Test T1-B in support of FFTF.

- 7) Initiated Control Rod Drive Mechanism (CRDM) roller-nut test in support of FFTF.
- 8) Continued liquid and vapor sodium sliding wear tests in support of FFTF.
- 9) Initiated test plans for In-Vessel Fuel Handling Machine (IVFHM) drive screw and wear pad tests in support of FFTF.
- 10) Initiated Alco IHX and Alco/BLH steam generator test in SCTI.
- 11) Diagnosed cause of Alco steam generator tube failure, coordinated with industrial contractors, effected repairs, and resumed testing.
- 12) Installed SCTI full-flow demineralizer; completed preparation for installation of SCTI Data Acquisition System (DAS).
- 13) Initiated SCTI Upgrade Modification Title I effort with Architect-Engineer (A-E).
- 14) Performed technical management of development contractors for 12 RDT contracts; includes FFTF development components.
- 15) Initiated seven LMEC subcontracts to industry for LMFBR technical support and development.
- 16) Structured R&D program for three LMFBR steam generator contractors.
- 17) Initiated SCTL (LCTL modification) Title I effort with A-E.
- 18) Initiated Title I effort for Pump Bearing Test Facility (PBTF) with LMEC as the A-E.
- 19) Completed initial concept Title I SPTF design.
- 20) Completed two pump seal test rigs.
- 21) Developed welding, removal, and reinstallation technique for 900°F strain gage.
- 22) Completed five Overall Conceptual System Design Descriptions (OCSDD's) for sodium test loops and rigs.
- 23) Completed drafts of LMFBR Piping Design Guide by C F Braun & Co under LMEC direction.

- 24) Responded to 525 requests for sodium technology information; abstracted and computerized 933 documents for instant recall.
- 25) Released Na-NaK Engineering Handbook final manuscript to publisher.
- 26) Continued Sodium Technology Training Program.
- 27) Published multivolume Failure Data Handbook for Nuclear Power Facilities.
- 28) Developed improved choking theory for two-phase flow in Na-H₂O reaction small-leak studies.

The immediate purpose of the LMEC is to support the LMFBR program by supplying detailed technical effort to the LMFBR development program. To accomplish this, the LMEC program is divided into technical and administrative projects according to special requirements of the LMEC and the LMFBR Program Plan. The relationship of each of these projects to the LMFBR Program Plan is indicated in the following table.

LMEC REFERENCE TO THE LMFBR PROGRAM PLAN

Project Subaccount No.	LMEC Project	LMFBR Program Plan Reference
12100*	AEC Assistance and Consultation	Directly or indirectly related to all tasks.
12100*	Codes and Standards	2-2.3, 3-2.2
12107	Maintenance and Malfunction Analyses	2-1.7, 2-1.9, 3-2.1.3, 3-2.2, 3-2.4, 3-4.3
12109*	Quality Assurance	2-2.3, 3-2.3, 3-4.2, 3-4.3
12200*	LMFBR Test and Facility Engineering, Planning, Coordination, and Evaluation	3-5.1, 3-5.2, 4-1.4, 5-2, 5-3, 5-9
12300*	Liquid Metal Pump Systems	3-11
12400*	Valve Contract Management and Testing	3-8.5
12500*	LMFBR Steam Generator and Intermediate Heat Exchanger Systems and Contract Management	3-6, 3-12, 4-3
12600*	Piping Systems and Contract Management	3-4.3, 3-8.2, 3-8.3, 3-8.4, 3-8.6
12700	Sodium-Water Reaction Study	3-6.1.1, 3-6.4, 3-6.5.3
13200	Operation of Sodium Components Test Installation (SCTI)	3-5.1, 3-6.1, 3-6.5, 3-12.1, 3-12.4
13300*	Liquid Metals Information Center	All elements except Element 9
13400	Sodium Pump Test Facility (SPTF)	3-11, 3-11.5, 3-11.5.3
14100*	Sodium Pump Seal Test Program	3-11.3.1
14200*	Sodium Pump Bearing Test Program	3-5.2, 3-11.3.2
15100	Precision Mechanisms in Sodium (PMIS)	3-3, 3-7.3, 3-10.3, 3-11
15200	Materials Compatibility in Sodium (MCIS)	5-1.6, 5-1.8
16200	Steam Generator and Intermediate Heat Exchanger Performance Evaluation	3-6, 3-12
17100	Instrumentation Testing, Evaluation, Calibration, and Standards	4-1 through 4-7
17110*	Strain Gage Development Contract Technical Management	4-7.11
17120*	Pressure Sensor Development Contract Technical Management	4-7.7
17200*	Instrumentation and Standards Laboratory	4-1.4
89110	Chemical and Metallurgical Laboratory	3-3.1, 5-5.1 through 5-5.7
89300	LCTL/SCTL Operation, Maintenance, and Modification	3-5, 3-6, 4-1
89400*	Technical Liaison for Materials and Process Development	3-3.1, 3-4
89450*	Component Materials Management and Control	3-3.2

*These projects are identified as being administrative, general support, or contractor management in scope. Since they do not generate direct technical progress information of immediate use and interest to the LMFBR community, they are not included in this technical progress report.

SUMMARY

SODIUM-WATER REACTION STUDY

An elementary computer model of the "small-leak" process has been developed to assist in interpretation of experimental data and the selection of significant test parameters. The model is a one-dimensional, adiabatic, homogeneous-equilibrium representation for impact of droplets in a supersonic jet of flashing water against a compressible solid surface. It permits variation of: (1) reservoir water conditions, (2) length-to-diameter ratio of the leak passage, (3) back pressure, and (4) target material properties. The model incorporates an improved two-phase-flow "choking" theory recently developed by LMEC.

OPERATION OF SODIUM COMPONENTS TEST INSTALLATION (SCTI)

Investigation of the steam generator tube leak was continued. A single defective tube was found, and the leak location was established. Repair was effected by plugging the defective tube and also a second tube with a cracked fillet weld at the upper tube sheet. During the tube repair activities, cracks were found in the overlay material of the ring groove of the steam generator upper manway flange. Cracking has been attributed to the high radial loading and thermal cycling. The cracks were removed by machining and the Inconel overlay was repaired. Specially designed seal-welded diaphragms were installed across the manway openings to replace the original ring joint seals. Water-side repairs on the steam generator were completed early in December and steaming operation was resumed on December 29, 1969.

Additional failures were experienced in the bearing and seal housing of the primary sodium pump. Several changes have been made to mechanical components and to the method of operation, which are expected to improve future performance.

SODIUM PUMP TEST FACILITY (SPTF)

The SPTF is being designed to perform steady-state and thermal transient testing of large sodium pumps and components for the LMFBR program.

Facility configuration is continuing to be investigated. The design bases and performance criteria have changed. The design pressure was increased from 225 psig to 250 psig. The design temperature was changed from 1200°F to 1100°F. One 18-in.-diameter test loop is being considered instead of two 30-in.-diameter test loops.

Investigations are continuing in the areas of secondary cyclic piping stress, control valve cavitation, atmospheric sodium oxide release, and sodium flow accuracy requirements to satisfy FFTF pump test requirements and LMFBR program requirements.

PRECISION MECHANISMS IN SODIUM (PMIS)

In support of a test request (T-20) from BNW/FFTF, testing of an N. S. Savannah Control Rod Drive Mechanism was initiated. Testing of the mechanism over 500°F argon and 600°F sodium has been completed. To date, the mechanism has been successfully cycled 200 times (a cycle being a 3-ft withdrawal to the out position, a 3-ft insertion, a 3-ft withdrawal, and a scram). A 1-week dwell period over 600°F sodium has been initiated in accordance with the test plan.

MATERIALS COMPATIBILITY IN SODIUM (MCIS)

Sliding wear tests have continued in 1200°F sodium in support of FFTF with the most promising materials: Inconel 718, Udiment 630, Hastelloy C, and Stellite 1016.

Tests are now in progress in support of the In-Vessel Fuel Handling Machine (IVFHM) and the fuel duct wear pads for FFTF.

STEAM GENERATOR AND INTERMEDIATE HEAT EXCHANGER PERFORMANCE EVALUATION

The plant performance tests include steady-state periods at 20 and 40% of rated power. Evaluation of data for these tests disclosed a drastic and anomalous drop in performance from 20 to 40% power. From these data, the steam generator appears incapable of performing at its rated power. Analyses of the cause of heat transfer have not yet led to a conclusive explanation. Instability, fouling, and rise-to-power procedure are among the possible causes being investigated. IHX performance appears to be satisfactory.

A diagnostic test program to investigate certain possible causes of the poor heat transfer has been formulated. The regular steady-state test program has been deferred pending the results of this program.

After a tube leak caused a shutdown of the steam generator, an investigation of its cause and results was undertaken. A topical report covering the results of this investigation is being published. Inspection pertaining to the tube leak also disclosed cracks in the upper manway seal groove. Investigation has provided fairly conclusive explanation of this defect. The cracks resulted from the combination of an improper ring seal design and subsurface metallurgical defects in the Inconel overlay. The failure investigations investigations included a compilation of a thermal history of the steam generator.

Temperature anomalies continue to be observed in the steam generator lower tube sheet area. The gas blanket was found to provide satisfactory thermal protection for the upper tube sheet.

The problem of operating the steam generator so as to obtain the most useful strain gage measurements is under study.

INSTRUMENTATION TESTING, EVALUATION, CALIBRATION, AND STANDARDS

The most significant instrumentation accomplishments during this reporting period have been: (1) finalizing the development of the strain gage calibration, removal, and reinstallation technique; (2) completion of the design and fabrication of the strain gage sodium test capsule; and (3) completion of the strain gage vibration test. (Data are in process of evaluation.)

LCTL/SCTL OPERATION, MAINTENANCE, AND MODIFICATION

Reexamination and evaluation of defects in the 3-in. loop piping resulted in acceptance for use during the FFTF T-1 tests. During examination, a minute sodium leak was found; the affected pipe section was replaced.

Loop modifications for Test T-1B were completed, new pumps were installed and performance tested, the T-1B test assembly was installed, and testing was started. Steady-state testing was started after module flowrates were measured at specified pressures. A total of 1479.4 hr of testing was accumulated at the steady-state conditions of 750°F bulk sodium temperature and a test section ΔP of 140 psi.

A small sodium leak occurred adjacent to a weld in the plugging meter loop; defective materials were replaced and operation was resumed. Because of sodium freeze-stem valve malfunctions attributed to the accumulation of oxides on the stem, a modification was designed to exclude air from the portion of the stem exposed to sodium.

I. SODIUM-WATER REACTION STUDY

(LMFBR Program Plan 3-6.1.1, 3-6.4, 3-6.5.3)

K. L. Adler

A. INTRODUCTION

The objective of this project is to develop information on the effects of water or steam leaks in a sodium-heated steam generator and to provide guidelines for applying this knowledge in the design and operation of LMFBR systems and components. As part of this task, LMEC has been assigned responsibility for technical management of the sodium-water reaction program at Atomic Power Development Associates (APDA), Inc. Analytical work has been started at LMEC to investigate fundamental phenomena associated with a leakage jet in order to define a rational approach to the interpretation of experimental data and to the design of scaled sodium-water tests and meaningful simulation experiments.

B. COMPUTER SIMULATION OF WATER JET

To assist the interpretation of experimental data and the selection of experimental conditions, an elementary computer model of the small-leak process has been created. This one-dimensional, steady, adiabatic, homogeneous-equilibrium model for the impact of droplets in a supersonic jet of flashing water against a compressible solid surface assumes: (1) an isentropic expansion and Fanno (adiabatic, constant area) process to a choked state (defined by the transition from subsonic to supersonic conditions), then (2) an isentropic expansion to a stationary normal shock wave, followed by (3) an isentropic compression to the throttled state. The water phase is assumed to have zero slip up to the standing bow shock and to be in the form of unretarded droplets in the shock layer. A linear relationship between the stopping shock velocity and particle velocity is assumed for both the water and the target material. Because a one-dimensional analysis cannot yield any angles, only the relative cross-sectional areas at various planes of interest can be calculated, but not their spacing. Also, neither reaction nor mixing at the jet periphery has been considered. However, even such a primitive model does permit variation of the initial water conditions, the length and diameter of the leak passage, the back pressure (in the sodium), and properties of the target material.

Throughout the steady portion of such calculations, the stagnation enthalpy must be conserved because of the adiabatic assumption. The desired reservoir (initial) pressure and temperature determine the stagnation enthalpy and the initial entropy, while the stagnation enthalpy and the back pressure determine the final entropy. The required entropy rise is assumed to be entirely due to friction in the capillary nozzle and to a standing normal shock wave in the emergent supersonic jet. (Extremely high back pressures are not considered.) If the entropy increase across the nozzle is assumed, then the choked state is determined by the rule (as recently broadened by LMEC) that homogeneous-equilibrium choking in an expansion corresponds to the transition from subsonic to supersonic conditions, which occurs either at saturation or inside the "dome" where the Mach number reaches unity. The next major step is the determination of that normal shock which will produce the remainder of the required entropy rise, while satisfying the requirements for conservation of mass, momentum, and energy fluxes. This determines the velocity and mass flux ahead of the shock, in particular; and the ratio of the mass flux at the choked state to the mass flux ahead of the shock gives the ratio of the cross-sectional area of the jet at the shock location to that at the nozzle exit. All of these steady calculations are based upon the 1967 International Formulation Committee (IFC) document, "Formulation of the Thermodynamic Properties of Ordinary Water Substance,"* as are the auxiliary calculations which permit interpretation of the chosen nozzle entropy increase in terms of the length and the diameter of the capillary.

A steady flow against the target would stagnate at the assumed back pressure, but a pulsating flow can produce impact pressures several orders of magnitude higher (the water-hammer effect). Also since bow shock standoff distances are typically less than the target's effective nose radius, the shock layer is expected to be so thin that negligible deceleration of the droplets occurs before impact. Thus, the final portion of the model is a transient analysis of the interaction of the target and the unretarded water droplets. For these calculations, the necessary dynamic properties are found in "Compendium of Shock Wave Data," UCRL-50108. In particular, the sound speed, slope of the shock

*C. A. Meyer et al., 1967 ASME Steam Tables, 2nd Edition (Am. Soc. of Mech. Engrs., New York, 1968)

velocity-particle velocity curve, and the density of both the water and the target material are required at the pressure-temperature conditions of the water and of the surface just before impact. Permanent damage to the surface is predicted when the impact pressure is calculated to exceed one to three times the yield strength of the target material. Since the droplet size has not been estimated, neither the duration of the impact pressures nor the scale of the individual pitting can be calculated by this initial model.

Table 1 is a typical printout from the present small-leak program. The assumed reservoir conditions of 628°F and 2650 psig were selected from conditions reported used by the Atomic Power Development Associates (APDA) in their small-leak experiments. The assumed back pressure (P_{20}) of 2 atm is also typical of APDA conditions. The assumed length-diameter ratio of the capillary nozzle (about 25 times the non-dimensional λ) is less than usually used by APDA, but this is not a very sensitive parameter since the entropy rise due to friction in the capillary is such a small fraction of the total required entropy increase.

In this output, T and P refer to temperature and pressure, respectively; H and S refer to specific enthalpy and specific entropy; and Q, U, M, and G refer to quality, velocity, Mach number, and mass flux. The subscripts 1 and 2 refer to conditions just upstream and downstream of the standing shock, respectively; and the subscripts zero and ch refer to the corresponding reservoir or choked states. Under the dual heading, M_{ch}/Q_{ch} , an entry greater than or equal to unity is to be understood as the choking Mach number, while an entry less than unity is to be taken as the choking quality (for this particular case, $Q_{ch} = 3.1\%$). The diameter ratio is the ratio of the diameter of the jet at the standing shock location to the effective diameter of the capillary nozzle. The kinetic energy of approaching water is the energy flux calculated as the mass flux at the shock location (G_1) times the local quality (Q_1) and $U_1^2/2$.

Included with the results of the steady supersonic jet calculations are the results of the transient impact calculations. The impact of a water droplet at the velocity U_1 is shown to impart an initial velocity of 177 ft/sec to the target surface instantaneously and to cause a transmitted shock wave to move forward through the steel, and a stopping shock wave to move backwards through the

TABLE 1
TYPICAL PRINTOUT FROM SMALL-LEAK PROGRAM

FINISH THE "SMALL LEAK" MODEL

INITIAL (RESERVOIR) CONDITIONS

T10	P10	H0	S1
DEG F	PSIG	BTU/LB	BTU/LB-F
628	2650	652.3	.84160

DOWNSTREAM STAGNATION CONDITIONS (THROTTLED STATE)

P20	H0	T20	S2	Q20
PSIA	BTU/LB	DEG F	BTU/LB-F	PCT
29.4	652.3	249.2	0.97967	45.9

CHOKED STATE (NOZZLE EXIT)

S CH	H0	LAMDA	T CH	P CH	G CH	M CH
BTU/LB-F	BTU/LB		DEG F	PSIA	PSF/SEC	Q CH
0.84406	652.3	1.1086	612.0	1686	9959	0.031

CONDITIONS AHEAD OF A STANDING NORMAL SHOCK

S1	H0	T1	P1	U1	Q1	DIAMETER
BTU/LB-F	BTU/LB	DEG F	PSIA	FPS	PCT	RATIO
.84406	652.3	129.0	2.17	2870	38.3	19.7

KINETIC ENERGY OF APPROACHING WATER

15.8 THOUSANDS OF FT-LB/SI-SEC

CONDITIONS BEHIND A STANDING NORMAL SHOCK

S2	H0	T2	P2	U2	Q2
BTU/LB-F	BTU/LB	DEG F	PSIA	FPS	PCT
0.97967	652.3	245.9	27.8	315.9	46.0

MOMENTARY PRESSURE UNDER A STOPPED WATER DROPLET (FROM VELOCITY U1)

IMPACT PRESSURE	WATER SHOCK	WALL VELOCITY	STEEL SHOCK
PSI	FPS	FPS	FPS
357,000	6980	177.3	18900

PEAK WORK TO WALL

63.2 MILLIONS OF FT-LB/SI-SEC

AVERAGE/PEAK

.000250

water, each traveling at the indicated velocity with respect to laboratory coordinates. The product of the pressure on the wall and the wall velocity gives the peak rate of delivery of mechanical work to the target per unit area due to the liquid impact. The ratio of the kinetic energy flux of the water ahead of the standing shock to this peak flux of work to the wall indicates the fraction of the impacted wall area which is subjected to the peak loading at any moment.

Exploitation of this small-leak model is continuing and a technical memorandum presenting and explaining the detailed computer program is being drafted. To date, the most interesting demonstration has been the sensitivity of the results to the assumed back pressure, particularly since that parameter has not been well-controlled in past experiments. For a decrease in back pressure of an order of magnitude, the area ratio of the jet is calculated to increase by an order, the kinetic energy flux of the water at the shock decreases nearly an order, the ratio of average-to-peak wall loading decreases by an order, the impact pressure increases by 50%, and the rate of work delivery to the wall doubles. Clearly, such experiments should be designed to maintain a known constant back pressure, or the observed time-integrated damage presumably will remain completely uninterpretable. Also, it appears that increased shell-side pressures might be considered as one way to decrease the rate of damage due to a small leak of water in a sodium-heated steam generator.

II. OPERATION OF SODIUM COMPONENTS TEST INSTALLATION (SCTI)

(LMFBR Program Plan 3-5.1, 3-6.1, 3-6.5, 3-12.1 3-12.4)

W. S. DeBear

A. INTRODUCTION

The primary objective of this project is to provide and operate a test facility for testing advanced prototype sodium-heated steam generators under simulated LMFBR operating conditions.

B. PLANT OPERATION AND MAINTENANCE

1. Systems Status

The primary sodium system remained in operation for approximately 4 months of the report interval with one major outage for repairs to the bearing and seal housing of the primary sodium pump (P-5). The secondary sodium and steam and feedwater systems remained inoperative for most of the interval during repair of the Alco/BLH steam generator. These repairs were completed early in December, secondary sodium circulation was resumed on December 22, and a steaming cycle was achieved on December 29, 1969. Prior to admission of water to the steam generator, feedwater circulation had been established through the steam generator bypass to check out the new full-flow demineralizer and to establish acceptable water conditions for resumption of normal operation. System operation and major maintenance activities are indicated on Figure 1.

2. Steam Generator Repairs

The helium leak tests previously reported were continued at the outset of the interval using improved techniques to provide a more definitive indication of the actual leak location. These tests confirmed that leakage existed only across a single tube; they also established an accurate leak location at approximately 6-1/4 in. below the top of the upper tube sheet. The previously reported location, based on a crude detection procedure, was 19 ft below the tube sheet. The major improvement which resulted in the final definition was the use of a carefully controlled purge of instrument air through the defective tube. This helped avoid ambiguity resulting from diffusion of helium through the tube, and from the pumping action caused by axial movement of the leak-detector sensing

Figure 1. SCTI Schedule of Events, July through December 1969

tube. The final defect location was confirmed by a second leak check procedure which permitted the isolation of various segments of the tube above and below the source of helium leakage.

Following positive identification of the defective tube a cleaning procedure was implemented to remove surface deposits. Surface materials were collected and were submitted for analyses by chemical and X-ray detection methods. The only materials present were found to be Fe_3O_4 and Fe_2O_3 . No reaction products were found, indicating that any sodium input to the water side was small and had been completely removed during the brief water circulation interval prior to leak detection.

Extensive eddy current testing was conducted as an independent means of finding and characterizing the defect, and also to search for possible wastage of adjacent tubes. Initial testing was conducted using equipment which had been developed by Atomics International (AI). Although this equipment could detect defects in a calibration standard, available circuitry did not permit complete differentiation between defects and external deposits such as might exist from residual sodium and/or reaction products.

Subsequent to the initial eddy current tests, a contract was established with Southwest Research Institute (SRI) to conduct additional eddy current surveys. The SRI testing confirmed the location of the defect and characterized it as predominantly circumferential, with an axial component of not more than 1/16 in. The SRI surveys included each tube location at the upper and lower tube sheets to assure that no other defects or potential defects were present at the same location as the known defect. In addition, a representative sampling of tubes was probed over the full tube length. Particular attention was given to those tubes which were potential targets of leakage from the defective tube. No other defects or wastage indications were found.

Visual and borescopic examinations of the defect region were conducted using both rigid and fiber optics borescopes. After numerous attempts, visual sightings were achieved and a number of photographic studies were successfully accomplished. They indicated the presence of a surface irregularity at the elevation of the defect, which was reported to have a corroded or eroded appearance. A jagged crack also was evident running through the central region of this

irregularity. Figure 2 is representative of the photographic coverage through the 12-in. rigid borescope.

The visual examinations were supplemented by obtaining a flexible plastic casting (Siligel) of the upper 7 in. of the defective tube. The casting technique reproduced surface conditions adequately enough to show the crack and the other indications in the region of the defect. It also showed the expanded diameter where the tube was rolled to the tube sheet. A typical casting is shown in Figure 3 with several of the key features identified. The particular casting photograph shows a complete crack at the defect location. This occurred on removal of the casting and is not representative of the actual size of the defect within the tube.

All tube-to-tube-sheet welds on both upper and lower tube sheets were inspected for cracking using dye-penetrant examinations. Two crack indications and one porosity indication were obtained during these inspections. Subsequent examination showed that only one of the three indications, a circumferential crack near the top of the tube-to-tube-sheet fillet weld, was significant. Repetitive grinding and dye-penetrant examinations indicated that crack projection was downward through the overlay material toward the tube sheet overlay surface. Supplemental cracks also were found to propagate radially toward the tube wall as the depth of the main crack increased (Figure 4). Following these observations it was decided to plug this tube as well as the tube with the wall defect.

Tubes 2-47 (defective tube) and 4-27 (cracked fillet weld) were plugged, following removal of the tube projection above the tube sheet, by spot facing. The latter operation was used to provide approximately a 1-in.-diameter seat for the Inconel plugs which were then seal welded to the tube-sheet overlay material. Prior to their installation the plugs had been analyzed for normal pressure loads and also for the effects of thermal transients to assure adequate strength and fatigue life. Figure 5 shows the final plug installation in tube 2-47.

3. Steam Generator Manway Flange Repairs

Early in August, after the tube repair effort was nearing completion, the ring-joint metal O-rings were removed from the steam generator manway flange openings. These rings had been left in place during the early repair activities to provide protection to the ring-joint grooves. Following removal, inspections

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Figure 2. Crack Indication Through
12-in. Rigid Borescope

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Figure 3. Flexible Casting of Upper Portion of
Tube 2-47

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Figure 4. Circumferential Crack in Fillet Weld of
Tube 4-27

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Figure 5. Plug Installation in Tube 2-47 (Defective Tube)

of the ring joint grooves revealed extensive circumferential cracking at the upper flange. Crack distribution and severity is illustrated in Figures 6 and 7. Cracks occurred at the base of the groove and near its inner diameter. The inner lip of the flange was found to be deformed (radially inward) increasing the side angle of the groove by an estimated 3 to 6 deg. Similar deformation was evident on the inner lip of the lower manway flange ring joint, but no cracking was found in this region.

Various inspection techniques were applied to establish the severity of cracking. Radiographic inspections were unsuccessful, but ultrasonic testing did indicate a crack penetration of approximately 125 mils. Similar penetration was obtained from the insertion of 3-mil wire probes into the largest of the cracks. Subsequent to these observations LMEC recommended controlled machining of selected crack regions to establish the actual depth and direction of propagation of the cracks. Representative photographs obtained during the incremental machining procedure are included as Figures 8 and 9.

Initially, three crack regions were selected for exploratory machining. Machining was accomplished in increments of 10 mils and later in larger increments of approximately 25 mils. After the first few exploratory studies the cleanup machining was extended to encompass all of the cracks evident in the manway flange. With few exceptions, most of the cracks were eliminated near a depth of 125 mils. However, the largest crack, at the 2 o'clock position, extended to a maximum depth of approximately 195 mils. Throughout the machining operation chip samples were retained and were submitted for analyses to determine the extent of penetration through the transition zone of the Inconel overlay material, and to protect against penetration into base metal. The information in Table 2 is a representative survey of the chip constituents as a function of penetration depth. Data were obtained at the 2 o'clock position and are indicative of results associated with maximum penetration. As can be seen, the crack removal operation was accomplished entirely within the overlay transition zone. The maximum concentration of stainless steel in the chip samples was of the order of 40 to 45%.

During the course of the crack elimination efforts a boat sample was obtained containing a complete crack segment. The sample was procured from a crack at the five o'clock position (Figure 10). Metallurgical examinations of

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Figure 6. View of Top Manway Flange Showing Cracks in the Bottom of the Groove

Figure 7. Closeup of Crack at the 2 O'Clock Position

6UN37-8/29/69-S1A

LMC-70-1

6UN37-8/29/69-61B

Figure 8. Closeup of Crack at 2 O'Clock Position after Machining to a Depth of 0.025 in.

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6UN37-8/29/69-S1G

Figure 9. Closeup of Crack at 2 O'Clock Position after Machining to a Depth of 0.125 in.

Figure 10. View of the O-Ring Seal Groove on Top Manway Flange

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TABLE 2
ANALYSIS RESULTS ON MANWAY FLANGE
CHIP SPECIMENS

Depth (mil)	Chip Constituents (%)					
	Mo	Nb	Ni	Fe	Cr	SS*
0.25	0.4	2.0	68	15	14	18
0.75	0.5	2.0	67	16	14	20
0.100	0.7	1.9	62	20	15	26
0.110	0.9	1.5	55	27	15	37
0.125	0.9	1.6	54	28	16	38
0.137	0.9	1.8	57	25	15	34
0.150	0.9	1.7	55	27	16	37
0.180	1.0	1.5	51	31	15	43
0.195	0.9	1.5	54	29	15	40

*The percentage of stainless steel was obtained from measured percentages of iron and molybdenum in the chip analyses, ratioed to the expected percentages of these constituents of Type 316 SS.

the boat sample showed subsurface microfissuring predominantly in the first layer of overlay material. A macroscopic view of the boat sample and several photomicrographs are included as Figures 11 through 13. The large tensile-type crack which propagated through to the overlay surface (Figure 12) apparently was initiated from one of these original microfissures.

Machining of the ring joint for crack elimination was accomplished using a 1/4-in. -diameter end mill. Following crack removal the machined surfaces were hand ground to eliminate vertical walls and sharp corners to establish an acceptable configuration for the overlay repair welds. These areas were then covered with an Inconel overlay to reestablish a full protective layer above the base metal. The final appearance of the overlay repair is shown in Figure 14. As described below, the method of sealing the manway flanges was changed to eliminate the ring joint. Consequently, no machining of the overlay repair areas was required, other than surface polishing to permit completion of dye-penetrant weld acceptance tests. Following completion of the welding activities, the manway flange surfaces, the overlaid region of the manway covers, and the inside surfaces of the manway flange openings were inspected using high resolution,

Figure 11. Macrograph of Crack in the O-Ring
Groove of SCTI Steam Generator Outlet
Flange (Mag: 8X)

post-emulsified fluorescent dye penetrant. This work was performed to investigate the possibility of crack network propagation within the overlay material. Results were satisfactory; no cracks or crack networks were detected.

Consideration was given to several alternatives for repair and sealing of the steam generator manway flanges. Initial attention was concentrated on rehabilitation of the original ring-joint seal, and on the addition of a stiffener ring inside the manway flange to avoid recurrence of the distortion problem. This approach would have necessitated extensive field machining on the steam generator and would have been extremely costly and time-consuming to implement. The second approach, which was finally selected, was based on utilization of a simple, seal-welded diaphragm. Several diaphragm designs were analyzed, including convoluted designs which would avoid shear loadings on the seal weld, but the final selection was a simple flat plate with a reduced-thickness central section to minimize radial loads. In addition, the sealing area was designed to provide sufficient frictional resistance to minimize shear loadings at the seal weld. The final diaphragm configuration is illustrated in Figure 15. As shown, this design utilized 16-mil-deep serrations on the sealing surface to improve sealing capability and to enhance the frictional resistance between the diaphragm

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Figure 12. Microstructure of the Inconel Overlay
(10% Oxalic Etchant, Mag: 100X)

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Figure 13. Microstructure of the Inconel Overlay in a Section without any Visible Surface Crack, Showing Subsurface Intergranular Fissuring. (This section was about 0.03 in. away from the main crack; 10% oxalic acid, Mag: 75X)

and the mounting flange. The outside surfaces in contact with the manway covers received a fine finish and were lubricated with graphite powder, prior to placement of the manway covers, to accommodate relative motion caused by differential thermal expansion. The sealing surfaces of the manway flanges also were improved by lapping to increase the probability of achieving an effective seal. The final condition of this sealing surface (outside the ring-joint groove) can be seen on Figure 14.

Two manway closure diaphragms were fabricated and, following extensive cleaning of the steam generator tubes and the steam and feedwater chests, were tack welded to the manway flanges. Steam and feedwater chest condition prior to closure, and the diaphragm installations, are shown in Figures 16 and 17. The manway covers then were installed and a hydrostatic test was performed to a maximum pressure of 2225 psig to demonstrate the sealing effectiveness of the diaphragm as a simple gasket. The hydrostatic test was successful; no evidence of leakage was observed. The covers then were removed to permit

Figure 14. Final Appearance of the Overlay Repair

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Figure 15. Manway Closure Diaphragm

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7693-59106

Figure 16. Steam Chest Condition Prior to Closure

Figure 17. Feedwater Chest Condition Prior to Closure

completion of the full seal weld (Figures 18 and 19). The high resolution, dye-penetrant studies were repeated after seal welding and, again, no significant indications were obtained. Flange repair efforts were completed with the final installation of the manway covers.

4. Steam Generator Shell-Side Modifications and Sodium Flush

A helium leak check was conducted on various pieces of steam generator service equipment preparatory to sodium flushing to eliminate residual reaction products produced in the cover gas region by the tube leak of June 1969. Helium leakage was detected both at the flange and across the seat of the upper rupture disk (RD-1). This flanged unit was removed and replaced with a welded assembly such as is used at the lower end of the steam generator. The downstream side of the original disk, with a centrally located defect, is shown in Figure 20. The disk currently is undergoing metallurgical analyses to determine the cause of failure.

The replacement of RD-1 afforded an opportunity to visually inspect upper regions of the shellside of the steam generator. Representative views of the tube bundle are shown in Figure 21. Figure 22 is an upward view of the upper guide ring and bottom surface of the upper tube sheet. Figure 23 is a view down the tube bundle showing some deposition in the region of the sodium-cover gas interface. These photographs show the tube bundle to be in good condition. Tubes and surfaces in the cover gas region were free of sodium film, undoubtedly as a consequence of steam cleaning which occurred during the tube leak. Only a minor spattering of sodium-sodium oxide deposits are evident. Spattered material removed from the vicinity of the rupture disk opening was predominantly metallic sodium with a light oxide film on the surface.

Several changes were made to the steam generator cover gas vent system to satisfy requirements of the sodium flush procedure. The vent line manifold was cut and a freeze trap with purge and vent connections was installed to permit controlled raising of sodium level to the vicinity of the upper tube sheet. (The normal purge and vent paths, through vapor traps, were made inoperative during the flush by closing argon block valves. This was done to lock gas in the vapor traps to prevent their flooding by liquid sodium. The block valves had previously been helium leak checked to assure adequate tightness.) Electric

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Figure 18. Manway Flange Diaphragm Installation

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Figure 19. Diaphragm Seal Weld

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Figure 20. Downstream Side of Upper Rupture Disk RD-1
with Centrally Located Defect

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Figure 21. View of Steam Generator Tube Bundle
(Sheet 1 of 2)

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Figure 21. View of Steam Generator Tube Bundle (Sheet 2 of 2)

7693-40195

Figure 22. Upper Guide Ring and Under Surface of Upper Tube Sheet

7693-40192

Figure 23. Sodium Deposition in the Region of the Sodium-Cover Gas Interface

heaters also were added to the vent lines to prevent freezing of sodium during raised level operation and to promote complete drainage at the end of the procedure. The freeze trap modification proved quite effective and it has been retained as a permanent addition to the steam generator shell-side vent system.

The sodium flushing procedure (SCTI-SP-99) was implemented on December 26, 1969. Prior to raising sodium above normal operating level the secondary system had been cold trapped until the plugging temperature had been reduced below 240°F. After the level increase a plugging temperature of 284°F was obtained. Cold trap operation was continued with the raised sodium level until the plugging temperature had been reduced to 245°F. This was considered a satisfactory condition for termination of the flush procedure.

5. Primary Sodium Pump Maintenance

The primary sodium pump was shut down for inspection on October 2, 1969 (after 2584 hr of operation) because of increased noise and vibration levels, increased bearing temperature, and progressive darkening of lubricating oil samples.

On disassembly, damage was found on the ball bearings and the lower stationary face seal. Bearing damage was most extreme on the outer race of the upper bearing (MRC 7214 DU) which carries the thrust in the back-to-back installation. Major damage was concentrated over approximately one-third of the race and appeared to propagate symmetrically from a central location. The lower bearing showed signs of running unloaded; there was some minor damage which probably resulted as a consequence of the upper bearing damage. The lower seal face was burred at both edges and apparently had worn or compressed approximately 1/32 in. The upper stationary face showed discoloration and had obviously been overheated but otherwise did not exhibit unusual wear.

Byron Jackson was requested to provide technical assistance in determining the cause and/or mode of failure. After review of the damaged parts and method of operation, they concluded that bearing failure had been induced by the passage of particulate material from the seal faces through the bearings. Lubricating oil is circulated by the lower impeller past the lower oil seal and back up through the bearings. Wear particles from the lower oil seal, therefore, are swept through the bearings with the attendant possibility of causing ball slippage and

local damage which eventually results in complete failure. This situation was aggravated by the lack of pressurization of the bearing and housing. The housing has always run at essentially ambient pressure, resulting in a pressure differential across the lower seal in excess of 30 psi. The higher pressure in the pump case tends to cause gas leakage (and particulate input) into the housing as opposed to the desired leakage flow of oil outward past the seal faces.

On the basis of their findings, Byron Jackson recommended modifications to the gas pressurization system to assure a balanced pressure across the lower oil seal. (The increased housing pressure also tends to promote oil leakage past the upper seal to provide adequate face lubrication.) LMEC personnel concurred with this recommendation on the basis of experience with similar sodium pump seal housings in SRE and HNPF. Additional recommendations were made relative to verifying adequate oil impeller discharge pressure, for monitoring bearing oil flow, and for measuring oil leakage rate past the face seals. All recommendations were incorporated during the subsequent housing reassembly.

The damaged bearings were returned to Merlin Rockwell Corporation (MRC) for examination and an evaluation of the cause of failure. Their results indicated an apparent thrust loading on the bearing equal to almost three times that which had been estimated from hydraulic considerations. In an effort to resolve this question thrust loading calculations were requested from several different sources. All calculations were in essential agreement after correction for the assumed dead weight of pump shaft and impeller. Finally, a totally independent loading evaluation, based on the measured width and location of the ball track in the thrust-carrying bearing, was performed. These data were submitted for computer analysis and the resultant thrust load was in excellent agreement (slightly lower) than the three calculated values (1300 lb vs 1500 to 1700 lb). Consequently, there is no information to substantiate an excessive thrust loading.

During the course of the above investigations, the pump shaft and inner barrel were removed from the pump case. These components were cleaned and the pump shaft was sent out for rebalancing. In the process of rebalancing, with the shaft supported on precision vee-blocks, it was determined that the top segment was bent relative to the main axis. An approximate 4-mil offset was measured between the keyway end of the shaft and the uppermost shoulder.

After this finding the shaft was returned to Byron Jackson for straightening. The slight offset was confirmed in their shop and the shaft was then pressed into an approximately true condition. After straightening, the measured runout at the keyway end was approximately 0.8 mil. Calculations were performed after this operation to establish whether or not additional rebalancing was necessary. Even with the most conservative assumptions the maximum imbalance was found to be less than 0.5 in.-oz. and the shaft was accepted for reuse.

Exactly when the upper segment of the shaft was bent is not known. This condition might have existed in the original assembly or it may have occurred during subsequent handling. Byron Jackson has been requested to evaluate its influence on bearing performance and to incorporate calculation results into their report on the bearing failure.

On December 9, 1969, bearing and seal housing reassembly was completed and the housing, inner barrel, and pump shaft were reinstalled. Inert gas system modifications then were completed to assure a balanced pressure between the pump case and the bearing housing. Gas connection to the housing was obtained upstream of a vapor trap to avoid contamination of lubricating oil by sodium vapor. Pressure gages were added to monitor housing pressure, and to measure oil impeller discharge pressure in the region of the upper seal. An interconnection was provided between the housing and the oil level sight gage to permit visual verification of oil flow. An oil filter was installed in this bypass line for removal of particulates. Additional provisions were made for the collection and measurement of oil leakage from both upper and lower seals, and for the addition of makeup oil to the housing without requiring depressurization. The pump was returned to service on December 10, 1969.

Pump operation was observed closely for several days following startup. Expected seal oil leakage was not obtained during initial operation but did begin after approximately 48 hr. The oil collected was black in color, and filterable material was collected from oil samples submitted for chemical analysis. Spectrographic analyses of the oil showed Cu, Cr, and Ba as major contaminants and Pb as a minor constituent; Cu, Cr, and Pb are constituents of the seal faces. Ba appears to be present in the oil and has been detected in previous samples.

Oil discoloration was limited to the oil which had leaked past the seal faces. Throughout the operation, oil circulating in the housing (and through the bearings) remained clear in color.

Because of the leakage condition it was decided to shut down the pump to investigate the seal faces; this was done on December 17, 1969. Byron Jackson and Borg-Warner (the seal supplier) participated in the examinations. The bronze stationary faces were inspected using microscopic equipment in the LMEC Metallurgical Laboratory. There was evidence of significant wear considering the extremely short operating interval. (Additional metallurgical analyses are being performed by both LMEC and Borg-Warner.) The rotating faces were scored as an apparent consequence of damage to the bronze faces.

The immediate conclusion of the Borg-Warner specialist was that bronze was, at best, a marginal material choice for the SCTI application, considering the pressure and spring loadings imposed. He indicated that bronze is extremely susceptible to damage from even momentary interruption of lubricating oil flow, such as might occur from the temporary accumulation of a gas bubble in the seal vicinity. It was recommended that carbon seals be substituted because of their: (1) much lower vulnerability to interruption of oil flow, and (2) higher compressive strength. No problem was foreseen with the rotating seals which are hard-faced with Stoddy Alloy. This recommendation is in accord with installation practice on similar sodium pumps. For example, the SRE pumps, which performed satisfactorily over extended periods, utilized stellite and carbon faces. Accordingly, the recommendation was accepted, carbon faces were procured from Byron Jackson, and the housing was reassembled and returned to service.

To avoid possible damage, the bearing set was not removed from the shaft sleeve during the outage. The entire assembly was clean and free of foreign material. Visible portions of the upper bearing were carefully examined and were found to be acceptable for continued service. Pump operation was resumed on December 23, 1969.

The feasibility of using available bearing and seal housings from SRE pumps is being reviewed in the event that further difficulties are experienced with the P-5 assembly. SRE experience with these units was excellent and they are of proper size for the SCTI application, although of somewhat different design.

C. TEST AND EVALUATION ACTIVITIES

A significant departure from expected performance was noted during operation of the Alco/BLH steam generator at the 40% load level prior to shutdown in June 1969. This was manifested as a decrease in heat transfer coefficient for the boiling regime. An extensive investigation was conducted into possible causes of the unexpected behavior. This work is reported in detail in Section VI of this report.

As a result of the above investigations a special series of diagnostic tests was proposed (TPL-04-PE-001, "Alco/BLH Steam Generator Boiling Phenomena Test Plan"), and suitable test procedures were developed. Testing was started on December 30, 1969. The initial test series is designed to investigate the effect of sodium-water ΔT on boiling phenomena. Subsequent testing will study the influence of water-side pressure drop on boiling behavior and also will determine the maximum capability of the steam generator at design steam conditions (2200 psig/1050°F). Earlier test results indicate a potential limitation at approximately 45% of rated load.

D. FUTURE EFFORT

The Alco/BLH steam generator boiling phenomena tests will be completed and will be analyzed to develop improved understanding of the behavior characteristics of the steam generator. Additional diagnostic testing may be proposed based on the results of the initial series of tests.

Strain gages will be installed on both the steam generator and intermediate heat exchanger to provide structural information on these components during subsequent testing activities. During the same outage, the DAS installation will be completed. An estimated outage time of 3-1/2 months will be required for the completion of these activities.

Follow-on testing plans and test procedures will be developed to cover testing activities through the end of calendar year 1970. Testing may encompass the original series of dynamic and transient tests or may be entirely revised based on findings and conclusions of the present diagnostic test results.

III. SODIUM PUMP TEST FACILITY (SPTF)

(LMFBR Program Plan 3-11.5)

S. L. Pfahler

A. SPTF EARTHQUAKE DESIGN INVESTIGATION

LMEC reviewed the SPTF earthquake design criteria, conducted a literature search, and contacted engineering firms in Los Angeles which have prepared structural designs for the Southern California area. The information obtained indicates that fossil-fueled power plants, chemical plants, and oil refineries are currently designed to the Uniform Building Code (UBC) requirements, and dynamic analyses are not performed.

LMEC's investigations did indicate that a dynamic analysis was performed for one building exceeding 40 stories. The analysis was not performed to verify the structural design or confirm UBC earthquake criteria, but rather for personnel safety considerations because of the possible magnitude of upper floor displacement. LMEC confirmed previous recommendations to base the SPTF design on the UBC requirements.

B. PIPING

The Title I design of the SPTF sodium piping systems was based on the February 1968 edition of the B31.7 code. This edition permits primary plus secondary stress intensities to exceed the allowable values up to 250 times under certain conditions. Some components in the PSDD SPTF design exceed the allowable values up to 175 times. A meeting held in Richland, Washington in May 1969 to establish the design basis for the FFTF sodium piping systems resulted in the decision to limit the number of times the allowable stress could be exceeded to 25 cycles. Recently, a high-temperature Code Case 67 for B31.7 indicated that a plastic analysis, rather than the elastic-plastic analysis, should be used above 800°F. The FFTF decision and this new Code Case have a large potential effect on the design basis for the full capability SPTF piping. For Alternate Z, the design temperature will be lowered to 1100°F and the thermal transient criteria will be reduced. It is expected that these changes will allow the design of the piping systems to proceed without delay.

C. CONTROL VALVES

The control valve selection is especially difficult because of the large pressure drop to be produced across the valves at all flow conditions. The two LMFBR valve study contractors have proposed completely different valve types for SPTF flow control. One valve contractor recommends using a new concept which consists of a concentric ported sleeve with multiple internal staging; this contractor also seriously questions the use of butterfly valves for this service. The other contractor recommends using four butterfly valves in series with the valve operators linked together to a common power unit. Several other major valve suppliers have also proposed but questioned the use of butterfly valves because of cavitation conditions. A definition of acceptable cavitation for butterfly valves in sodium service is required. At this time there are no reliable methods for predicting cavitation factors.

D. SAFETY

The safety analysis for the Title I design is based on assumptions not fully verified experimentally. The design of the SPTF includes provisions to minimize the potential concentrations of sodium oxide in the atmosphere in case of a large sodium release. These provisions consist of dikes and trenches to limit the burning area, a separation of main flow loops, and an arrangement of the loops in an essentially enclosed building. To provide further assurance that off-site concentrations do not exceed allowable limits, action was initiated to develop criteria and establish guidelines for realistically predicting off-site and on-site sodium-oxide concentrations.

Meteorological conditions at the LMEC test site could, at times, result in channeled or drainage flow between the site and Brandeis Camp; at these conditions, Pasquill's atmospheric diffusion equations are not applicable. A mathematical model was developed to describe the transport of the sodium oxide released to the nearest off-site inhabited area (Brandeis Campsite). This model included the assumptions of no pressure buildup inside the building and channeled flow.

E. FLOWMETER ACCURACY

LMEC conducted a survey to obtain information on pump and flow measurement accuracy requirements for the FFTF and LMFBR plant program. The purpose of the survey was to confirm the SPTF flow measurement accuracy design requirements.

The survey produced the following:

- 1) The opinion of the large plant designers is that: (1) for pump testing, a flow accuracy of ± 1.0 to 1.5% of measured flow is required; and (2) for flowmeter calibration, the flow accuracy of the SPTF master flowmeter or the calibration system should be ± 1.0 to 2.0% of measured flow.
- 2) For the FFTF pump tests, PNL indicated a flow measurement accuracy of $\pm 2.0\%$ or less was desired for pump and flowmeter testing.
- 3) The LMFBR Program Plan, Section 4, specifies requirements for test loop flowmeter accuracies of $\pm 2.0\%$. With this requirement of $\pm 2.0\%$ for the test loop meters, a master flowmeter or a calibration system would need an accuracy of ± 0.5 to 1.0% .

Flowmeter accuracy is affected by upstream flow disturbances, venturi distortions, and drift of the flowmeter differential pressure (DP) cells. To improve the accuracy of flow measurements in the SPTF, the following is being done:

- 1) A long straight run (>20 diameters) upstream of the venturi flowmeter is provided to eliminate upstream disturbances.
- 2) The venturi flowmeter will be precalibrated in water.
- 3) A means to calibrate NaK-filled DP cells in sodium service and to adjust them to their original calibration is presently being explored by LMEC.

LMEC calculated the SPTF overall flowmeter system accuracy using several assumptions. Figure 24 is based on the SPTF A-E's assumptions that only the flowmeter, the DP cells, the sodium temperature, and the Data Acquisition System contribute to the system accuracy. It was also assumed that the venturi

Figure 24. Modified FFTF Flow Loop (SPTF Overall System Accuracy)
PSDD Appendix 3 Assumptions

discharge coefficient determined in a water calibration is the same in sodium. As a result, the primary contributor to the system error is the accuracy of the DP cell; the predicted system error at 14,500 gpm on this basis is 0.65%. To establish Figure 25, LMEC considered six unknowns in the venturi calibration and five unknowns in the DP cell calibrations, and assumed maximum, median, and minimum values for each. On this basis, the median error at 14,500 gpm is predicted to be 2.55% as compared to 0.65% according to the A-E's method. In Figure 26, the effect of the long run upstream of the meter is assumed to act as a flow straightener and the DP cells are assumed to be calibrated in place. The median value for 14,500 gpm then becomes 1.90% with the long pipe run, and 1.70% using DP cell calibration.

Based on calculations by LMEC and the A-E, flow measurement requirements for FFTF pump performance testing can be satisfied with the water-calibrated, multiple-DP-cell, venturi flowmeter if in-place DP cell calibration can be accomplished.

For the SPTF to meet the accuracy requirements of large plant designers and to calibrate flowmeters for sodium test loops, it will be necessary to install flow calibration capability in the SPTF. The SPTF A-E has investigated a weigh cell installation on the feed and drain tank Phase II loop configuration. For flowrates up to 28,000 gpm (limited by the feed tank capacity), an accuracy of 0.7% of instantaneous flow is estimated. Above 28,000 gpm, indirect or bootstrap calibration is being considered.

Figure 25. Modified FFTF Flow Loop (SPTF Overall System Accuracy)
Utilizing Estimates of Twelve Sources of Error

7693-25138

Figure 26. Modified FFTF Flow Loop with Flow Straightener and ΔP Calibration (SPTF Overall System Accuracy) Utilizing Estimates of Twelve Sources of Error

IV. PRECISION MECHANISMS IN SODIUM

(LMFBR Program Plan 3-3, 3-7.3, 3-10, 3, 3-11)

J. H. Brindley

A. INTRODUCTION

Sodium coolant in the FFTF and in the LMFBR's will be used at or near 1200°F, resulting in removal of most oxide-bearing surface layers which provide corrosion resistance and, to some extent, lubrication and surface hardness. Thus, accelerated deterioration will be experienced on all engaged motion contact surfaces and may result in unexpected reactor shutdowns from component malfunctions or failures, thereby affecting power production costs quite severely. Thus, the importance of reliability in various mechanisms such as control rod bearings and gears or fuel holddown latches may be projected into a true economic perspective. Literature and experience surveys have shown that little data are available on material wear resistance in sliding, rolling, and fretting applications in sodium environments. Data peculiar to the design of mechanical devices for sodium service are insufficient to permit adequate design of elements for operation above 800 to 900°F. In view of the state-of-the-art and the potentially large financial benefits to be obtained by reducing reactor downtime, it is apparent that basic materials testing followed by prototype mechanisms tests must be performed.

The objective of the Precision Mechanisms in Sodium (PMIS) program is to obtain engineering information for the design of mechanical elements to ensure reliable operation in sodium liquid and vapor to 1200°F for the FFTF and for LMFBR's. The information obtained for bearings, gears, seals, latches, guides, spacers, connectors, and various mechanisms should permit a designer of fuel handling devices, control rod mechanisms, or core/fuel holddown devices to proceed with confidence as to such factors as allowable loadings and clearances, suitable material couples, friction, and wear factors.

B. EXPERIENCE SURVEY

An experience survey covering operating experiences with mechanical elements associated with the SRE, HNPf, EBR-II, and EFAPP reactor installations has been completed, is now being prepared for final publication, and will be published during the next report period.

C. MECHANICAL ELEMENT TESTS (CRDM Roller Nut Test)

In response to a test request (T-20) from BNW/FFTF, testing of a control rod drive mechanism (CRDM) in sodium vapor has been initiated at LMEC. A cutaway view of the mechanism is shown in Figure 27. The mechanism is a hermetically sealed, linear motion device consisting of a lead screw driven by a collapsible rotating and rolling type nut. The roller nut consists of four annularly grooved rollers, two of which are mounted on each of two segments of a pivoted motor-rotor. Each of the two rotor segments has two salient poles to form a four-pole rotor for normal rotation. Pivoting of the segments allows disengagement of the nut from the screw by interruption of stator current for emergency control rod insertion.

The objective of the test is to identify problems that this type of rod drive mechanism will encounter in a sodium environment and to provide basic data for the design of the FFTF reference prototype drive mechanism.

The drive mechanism is being operated in a vertical position over a vessel containing liquid sodium as shown in Figure 28. The liquid sodium temperature will be varied from 600 to 1200°F during the test program, while the argon cover gas laden with sodium vapors will be allowed to freely circulate within the drive mechanism intervals.

1. Test Program Status

The CRDM assembly and control console was received at LMEC from Todd Shipyards on October 15. Prior to test, the CRDM underwent a detailed dimensional inspection and X-ray of critical components in order to fully document the as-received condition of the mechanism. Disassembly, inspection, cleaning, and reassembly of the mechanism was accomplished in a laminar-flow clean room especially constructed by LMEC for this test program, as shown in Figure 29.

On November 24 and 25, the CRDM was reassembled and the roller nuts aligned with respect to the lead screw. This operation was performed by personnel from LMEC, BNW, Westinghouse, and Todd.

On November 25, the CRDM assembly, including the motor stator, was mounted in the assembly checkout stand adjacent to the sodium test loop to

7693-2570

Figure 27. N.S. Savannah Control Rod Drive Mechanism Operating Parts

Figure 28. PMIS Roller Nut
CRDM Test Design Layout
(Dwg.No.7693-PA15100210)

Figure 29. Laminar Flow Clean Room

7693-40210CN

perform room-temperature air tests, as shown in Figure 30. This test was required to verify functional operation of the mechanism prior to inserting it in the sodium facility. Malfunction of the control console prevented successful completion of these tests until December 12. The problem was found to be a loose setscrew preventing coupling of a shaft in a ganged assembly of three powerstats.

On December 3, after a 22-day delay, the Inconel X-750 buffer springs were received from the vendor and, as a result, test checkout of the buffer spring assembly (dashpot) in water was successfully completed on December 5. A schematic of the water test setup is shown in Figure 31, a photograph of the test apparatus is shown in Figure 32, and Figures 33 through 38 are photographs of the major components of the test piece.

As scheduled, on December 15, installation of the CRDM into the test facility was completed and heatup to 500°F was initiated for performance of the first phase of testing in argon. Photographs of the installation, before and after application of heaters and insulation, are shown in Figure 39. Photographs of major components of the CRDM are shown in Figures 40 through 42. Photographs of the control console and associated instrumentation are shown in Figure 43.

On December 16, a 500°F test temperature was reached and 10 cycles of the CRDM were completed. One magnetic proximity switch and 6 rotational sequence switches became inoperative, requiring bypass of the inhibit circuit in the console in order to perform the tests.

On December 17, the defective switches were replaced and the total system was checked out and found to be fully operational. The test vessel was then filled with sodium and a sample of the as-supplied sodium taken. Chemical analysis of the sodium revealed the oxygen content to be in the range of 4.1 to 12.1 ppm.

On December 18, the sodium temperature was brought to an isothermal condition of 600°F and cold trapping at approximately 275°F was started.

On December 22, a sodium sample was taken and chemical analysis showed the oxygen content of the sodium to be in the range of 17.2 to 41.3 ppm. It is postulated that these high results were due to the presence of minute metal shavings which contained oxides reduced by the sodium to form sodium oxide.

7693-55229A

Figure 30. Checkout of CRDM in Assembly Test Stand

Figure 31. Schematic of CRDM Buffer Spring Water Test

7693-55221A

Figure 32. Water Test of CRDM Buffer Spring
LMEC-70-1

7693-55228A

Figure 33. Buffer Spring Assembly

7693-55225A

Figure 34. Simulated Control Rod Weight

7693-55223

Figure 35. Buffer Spring Housing and Weight Guide Tube

7693-55224A

Figure 36. Control Rod Guide Housing
(Simulated FFTF Shield Plug Penetration)

7693-55226

Figure 37. Main Adapter Housing (CRDM to Test Facility)

7693-55222

Figure 38. Main Support Tube for Control Rod Penetration Region

Figure 39. CRDM Installed in Sodium Test Facility
(Prior to Application of Heaters and Insulation)
(Sheet 1 of 2)

7693-55230 CN

LMEC-70-1

7693-40208 CN

Figure 39. CRDM Installed in Sodium Test Facility
(After Application of Heaters and Insulation)
(Sheet 2 of 2)

LMEC-70-1

Figure 40. Stator Assembly

7694-55180A

LMEC-70-1

7693-55183 CN

Figure 41. Typical Roller Nut

LMEC-70-1

Figure 42. Motor Tube Housing, Spline Shaft, and Lead Screw

7694-55172 CN

Figure 43. Control Console and Instrumentation

As a result, the sodium test vessel was filled and drained three times to filter out the impurities in the sodium, and cold trapping was continued until December 30. Chemical analysis of a sodium sample taken on that date showed the oxygen content to be in the range of 6.0 to 12.0 ppm, thereby meeting the BNW test requirements of <15 ppm oxygen in sodium.

The required 200 run and scram cycles were successfully completed on December 31, and a 1-week hold (dwell) period over the 600°F sodium with the mechanism in the withdrawn position was initiated according to the test plan.

2. Test Data

Test data obtained during testing over 600°F sodium are as follows:

- 1) Drive coil input voltage = 90 vdc
- 2) Drive coil input current = 9 to 11 amps dc
- 3) Drive coil input power - 810 to 990 w
- 4) Scram response time (no change in time during run) = 0.37 sec total elapsed time
- 5) Drive rate = 9 in. /min, 36.25-in. stroke
- 6) Coolant water flowrate = <1 gpm
- 7) Inlet water temperature = ~65 to 70°F
- 8) Outlet water temperature = ~145 to 170°F
- 9) Drive coil (stator) temperature = 250 to 270°F
- 10) Total feet of travel for test to date = 1812.5 ft
- 11) Total scrams to date = 200
- 12) Drive line load = ~317 lb (weights and shaft but not lead screw).

A typical temperature profile for the test assembly during the 600°F tests is shown in Figure 44.

TEST TEMPERATURE: 600°F

DATE: JANUARY 1970

7693-25103

Figure 44. CRDM Test Assembly Thermal Profile

V. MATERIALS COMPATIBILITY IN SODIUM

(LMFBR Program Plan 5-1.6, 5-1.8)

J. J. Droher

A. INTRODUCTION

Sodium coolant in the FFTF and in LMFBR's will be used at or near 1200°F, resulting in removal of most oxide-bearing surface layers which provide corrosion resistance and, to some extent, lubrication and surface hardness. Thus, accelerated deterioration will be experienced on all engaged motion contact surfaces and may result in unexpected reactor shutdowns from component malfunctions or failures, thereby affecting power production costs quite severely. In view of the state-of-the-art (little information on material wear resistance in sliding, rolling, and fretting service) and the potentially large financial benefits to be obtained by reducing reactor downtime, it is apparent that basic materials testing must be performed.

The objective of the Materials Compatibility in Sodium (MCIS) program is to generate basic wear data which can be ultimately used in the design of mechanisms for operation in the sodium environment common to LMFBR's. The scope of the program will be defined by the requirements developed through Program Plan Task 3-3.1, "Materials Development Technical Liaison," and other technological requirements as they may occur. The information thus obtained will enable designers of bearings, gears, seals, latches, guides, spacers, connectors, and various mechanisms for fuel handling devices, control rod mechanisms, or core/fuel holddown devices to proceed with confidence as to such factors as allowable loadings, velocity, suitable material couples, friction, wear factors, and vapor deposition effects.

B. SLIDING WEAR TESTS

In support of FFTF Test Request T-8, Addendum 1, sliding contact wear tests were performed with flame-sprayed Inconel 718 in cold-trapped liquid sodium at 900 and 1200°F against itself, against flame sprayed Stellite 1016, and against 304 SS. Pertinent test data are reported in Table 3. These specimen materials were obtained from BNW, premachined for installation in LMEC test facilities.

TABLE 3
SLIDING WEAR TEST RESULTS
(FFTF Test Request T-8, Addendum 1)

Material		Sodium Environ- ment (cold trapped)	Sodium Temper- ature (°F)	Pin Sliding Velocity (cm/sec)	Load (lb)	Coefficient of Sliding Resis- tance at Test Temperature		Average Specific Wear on Pins (cm ³ /cm-k g x 10 ⁻¹¹)
Pin	Plate					Initial	Final	
Inconel 718*	Inconel 718*	Liquid	900	9.3,11.7,14.1	6.03,4.83,4.02	0.33	0.53	18.66
Type 304 SS	Inconel 718*	Liquid	900	9.3,11.7,14.1	6.07,4.85,4.04	0.22	0.46	18.07
Inconel 718*	Stellite 1016*	Liquid	900	9.3,11.7,14.1	5.94,4.75,3.96	0.27	0.40	43.44
Inconel 718	Inconel 718	Liquid	1200	9.0,11.2,13.5	6.03,4.82,4.02	0.42	0.29	Mat'l buildup
Type 304 SS	Inconel 718	Liquid	1200	9.0,11.2,13.5	6.06,4.85,4.04	0.40	0.33	21.2
Inconel 718	Stellite 1016	Liquid	1200	9.0,11.2,13.5	5.93,4.74,3.95	0.34	0.30	Mat'l buildup

*Flame-spray coating over a Metco 405 base coat over Type 304 SS base material

TABLE 4
SLIDING WEAR TEST RESULTS IN 1200°F SODIUM
(Sheet 1 of 2)

Material		Sodium Environ- ment	Pin Sliding Velocity (cm/sec)	Load (lb)	Coefficient of Sliding Resis- tance at Test Temperature		Average Specific Wear on Pins (cm ³ /cm-k g) x 10 ⁻¹¹)
Pin	Plate				Initial	Final	
Udimet 630	Udimet 630	Liquid*	16.25,20.33,24.42	1.50,1.20,1.00	0.93	0.20	Mat'l buildup
Udimet 630	Inconel 718	Liquid*	16.25,20.33,24.42	1.29,1.03,0.86	3.85	2.20	2.74
Inconel 718	Udimet 630	Liquid*	16.25,20.33,24.42	1.48,1.18,0.98	1.80	1.80	3.90
Udimet 630	Udimet 630	Liquid†	16.3,20.3,24.4	6.02,4.81,4.01	0.30	0.42	3.48
Udimet 630	Inconel 718	Liquid†	16.3,20.3,24.4	5.81,4.66,3.87	0.48	0.80	28.83
Inconel 718	Udimet 630	Liquid†	16.3,20.3,24.4	6.00,4.80,4.00	0.28	0.62	15.03
TZM	TZM	Liquid†	16.3,20.3,24.4	1.71,1.37,1.14	0.76	0.45	173.2, minor mat'l buildup
TZM	Inconel 718	Liquid†	16.3,20.3,24.4	1.22,0.97,0.81	2.52	0.66	Minor mat'l buildup
TZM	Udimet 630	Liquid†	16.3,20.3,24.4	1.46,1.17,0.98	0.93	0.57	Minor mat'l buildup
Udimet 630	Udimet 630	Liquid†	66.5,83.0,99.6	1.49,1.19,0.99	1.32	1.05	18.69
Udimet 630	Inconel 718	Liquid†	66.5,83.0,99.6	1.28,1.03,0.85	1.40	1.82	17.11
Inconel 718	Udimet 630	Liquid†	66.5,83.0,99.6	1.47,1.18,0.98	0.59	0.95	17.36
Inconel 718	Hastelloy C	Liquid*	66.5,83.0,99.6	1.21,0.97,0.81	1.48	0.71	1.82

TABLE 4
SLIDING WEAR TEST RESULTS IN 1200°F SODIUM
(Sheet 2 of 2)

Material		Sodium Environ- ment	Pin Sliding Velocity (cm/sec)	Load (lb)	Coefficient of Sliding Resis- tance at Test Temperature		Average Specific Wear on Pins (cm ³ /cm-kg x 10 ⁻¹¹)
Pin	Plate				Initial	Final	
Inconel 718	Hastelloy C	Liquid*	66.5,83.0,99.6	4.23,3.39,2.82	0.47	0.57	2.75
Hastelloy C	Inconel 718	Liquid*	66.5,83.0,99.6	1.21,0.97,0.81	0.88	0.99	25.41
Udimet 630	Udimet 630	Liquid*	66.5,83.0,99.6	4.09,3.27,2.73	0.37	1.55	109.92
Inconel 718	Udimet 630	Liquid*	66.5,83.0,99.6	3.88,3.11,2.59	0.51	0.75	46.45
Udimet 630	Inconel 718	Liquid*	66.5,83.0,99.6	4.08,3.26,2.72	0.67	0.41	106.86
Inconel 718	Hastelloy C	Liquid*	66.5,83.0,99.6	3.83,3.06,2.55	0.45	0.52	4.04
Inconel 718	Hastelloy C	Liquid*	66.5,83.0,99.6	6.17,4.94,4.11	0.41	0.41	12.27
Hastelloy C	Inconel 718	Liquid*	66.5,83.0,99.6	3.82,3.06,2.55	0.38	0.38	39.43
Inconel 718	Hastelloy C	Liquid*	33.2,41.5,49.8	3.82,3.06,2.55	0.33	0.37	11.30
Inconel 718	Hastelloy C	Liquid*	33.2,41.5,49.8	6.16,4.93,4.11	0.48	0.45	15.37
Hastelloy C	Inconel 718	Liquid*	33.2,41.5,49.8	3.81,3.05,2.54	0.31	0.38	101.14
Inconel 718	Hastelloy C	Liquid*	16.3,20.3,24.4	3.82,3.05,2.54	0.29	0.29	8.01
Inconel 718	Hastelloy C	Liquid*	16.3,20.3,24.4	6.15,4.92,4.10	0.25	0.27	15.54
Hastelloy C	Inconel 718	Liquid*	16.3,20.3,24.4	3.81,3.05,2.54	0.31	0.24	13.91

*Cold trapped

†Hot trapped

NOTE: ALL DIMENSIONS IN INCHES. MATERIAL IS 3-IN.-DIAMETER, SCHEDULE 10, TYPE 304 SS PIPE AND WELD FITTINGS.

7693-25104

Figure 45. Typical Cold Trap

7693-25106

Figure 46. Cold Trap X-ray

Specific wear values pertaining to flame-sprayed Inconel 718 were higher than similar rates determined for most other tests of these materials conducted in the past. Also, a slight warpage was detected on the Inconel 718 wear plates after the 900°F test. No warpage was observed after the 1200°F test. The flame-sprayed test specimens were more porous than cast or wrought materials, and were more difficult to clean after sodium exposure. These test results have been forwarded to BNW as an LMEC test report.

Additional tests conducted during the report period are presented in Table 4. Summary wear test data are presented in Tables 5 and 6; successful material combinations are tabulated as a function of increasing wear rate. Successful combinations are defined as material couples tested as both pins and plates and with which no galling or buildup was observed. In general, tests in sodium vapor yielded lower friction factors and wear coefficients, which is in agreement with test data obtained in England by W.H. Roberts.*

In preparation for performance of tests by LMEC in support of BNW/FFTF Test Request T-8, Addendum 2, two cold traps were removed from the sliding-wear test facilities. These traps were found to be plugged with sodium-oxide deposits. Figure 45 is a schematic of a typical trap. X-rays of the traps were taken prior to removal in an attempt to determine the existence of sodium-oxide deposits. Figure 46 is a typical X-ray showing the existence of a sodium-sodium-oxide conglomerate mixture. During trap cleaning, it was observed that the deposit remaining in the traps after the sodium was drained consisted of a porous matrix of sodium oxide which filled approximately 50% of the volume of the 3-in.-diameter cold trap along the finned length. The upper inlet line was clean of all sodium deposits. The sodium-oxide deposits began sharply at the uppermost fin location on the traps. Due to the geometrical configuration of the trap assembly after removal from the system, it was not practical to photograph the condition of the inside of the trap. The traps were steam cleaned, followed by a water flush, and reinstalled in the facilities. Sodium has been circulated through the stem and samples taken for sodium chemistry analysis. The oxygen content in the sodium was verified to be <25 ppm.

*W. H. Roberts, "Friction and Wear Behavior of Sliding Bearing Materials in Sodium Environments at Temperatures Up to 600°C," TRG Report 1269(R) (1966)

TABLE 5
SUCCESSFUL WEAR TEST MATERIAL COMBINATIONS
IN LIQUID SODIUM AT 1200°F

Material Combinations	Average Friction Coefficient (f)	Sum of Wear Rates $K_1 + K_2$ (cm ³ /kg-cm x 10 ⁻¹¹)
LT-2 - LT-2	0.19	No measurable wear
Udimet 630 - K-95	0.30	5.61
Hastelloy C - Stellite 1016	0.34	6.51
K-95 - K-95	0.52	6.86
Stellite 6B - K-95	0.62	7.61
TZM - Hastelloy C	0.33	9.37
Stellite 1 - Stellite 6B	0.38	13.05
Udimet 630 - Stellite 6B	0.48	13.19
Stellite 6B - K-801	0.37	14.97
Stellite 1016 - Stellite 273	0.31	16.72
Hastelloy C - K-95	0.43	18.80
Stellite 6H - Stellite 6H	0.41	19.48
Inconel 718 - Stellite 6H	0.35	31.16
Coors - Coors	0.72	35.8
Stellite 6B - Stellite 6B	0.49	48.34
Hastelloy C - Udimet 630	0.33	51.22
Hastelloy C - Inconel 718	0.35	53.14
Stellite 273 - Stellite 273	0.33	65.0
Hastelloy C - Stellite 6H	0.46	120.4
Inconel 750 - Inconel 750	0.48	673
Stellite 1016 - 304 SS*	0.38	2.88
Inconel 718 - Rex-49*	0.54	17.60
304 SS - 304 SS*	0.33	22.82

*Low-Temperature Tests (<1200°F)

TABLE 6
SUCCESSFUL WEAR TEST COMBINATIONS
IN SODIUM VAPOR AT 1200° F

Material Combinations	Average Friction Coefficient (f)	Sum of Wear Rates $K_1 + K_2$ (cm ³ /kg-cm x 10 ⁻¹¹)
K-95 - K-95	0.14	1.22
Inconel 718 - K-95	0.28	3.82
Hastelloy C - K-801	0.21	4.33
Hastelloy C - K-95	0.20	4.71
Udimet 630 - Udimet 630	0.21	4.90
Stellite 3 - K-95	0.31	5.96
Hastelloy C - Inconel 718	0.14	7.36
Stellite 1016 - K-95	0.38	8.13
Udimet 630 - K-95	0.29	9.18
Inconel 718 - Inconel 718	0.40	11.75
Hastelloy C - Udimet 630	0.18	14.03
Hastelloy C - Stellite 1016	0.31	27.37
Inconel 718 - Udimet 630	0.20	45.60
Hastelloy C - Hastelloy C	0.36	53.19
Stellite 6B - Stellite 6B	0.41	92.13
Stellite 6B - K-801	0.24	136.5
Inconel 718 - Stellite 6B	0.31	1010
Hastelloy C - Colmonoy 75*	0.37	1.17
Stellite 1016 - 304 SS*	0.45	1.44

*Low-Temperature Tests (<1200° F)

Testing has been initiated, as requested by T-8 Addendum 2, in support of development of the FFTF In-Vessel Fuel Handling Machine and the fuel duct wear pads. Basically, this testing consists of intermittent rubbing of various material combinations (Inconel 718, Hastelloy C, and Stellite 1016) in 450°F sodium, dwelling in 1050 to 1100°F sodium, followed by intermittent rubbing in 450°F sodium. Data to be obtained, include:

- 1) Wear coefficients.
- 2) Self-welding coefficients.
- 3) Gallling, brinelling, etc.

VI. STEAM GENERATOR AND INTERMEDIATE HEAT EXCHANGER PERFORMANCE EVALUATION

(LMFBR Program Plan 3-6, 3-12)

C. J. Kaplan

A. INTRODUCTION

Prototype models of intermediate heat exchangers and sodium-heated steam generators are to be tested in the Sodium Components Test Installation (SCTI) under the Sodium Component Development Program. The objective of the tests is to evaluate the performance of these heat exchangers so as to advance the development and design of reliable plant-sized units. At present, the units installed in the SCTI are an intermediate heat exchanger designed and built by Alco Products, Inc., and a steam generator designed by Alco and manufactured by Baldwin-Lima-Hamilton (BLH) Corp. The performance evaluation program had been previously conducted by the United Nuclear Corporation (UNC) and is now being continued by LMEC.

The Alco IHX is a vertical, counterflow shell-and-tube design, 28 ft long by 3 ft in diameter. It contains 462 tubes of Type 304 stainless steel, 1/2-in. OD by 0.035-in. wall thickness. The shell side contains 27 disk and donut baffles.

The Alco/BLH steam generator is a vertical, once-through, counterflow unit 42 ft long by 32 in. in diameter. It contains 300 tubes of 1/2-in. OD and 0.104-in. wall thickness, with a sine-wave bend at their upper end. The shell side contains 57 disk and donut baffles. The top tube sheet is protected by a 28-in. cover gas space. The bottom tube sheet is protected by a thermal baffle of ten 1/4-in. steel plates spaced 1/8 in. apart. It is made of Type 316 stainless steel with an Inconel coating on all steam-side surfaces.

The steam generator and IHX are each instrumented with approximately 100 thermocouples for measuring temperatures of the shell-side sodium and of various points in the structures. Figure 47 shows the location and identification of most of these thermocouples as well as the principal terminal measurement transducers of the units.

The SCTI, a facility constructed specifically for this type of testing, includes a primary sodium loop linking the IHX to a gas-fired sodium heater, a

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Figure 47. Alco/BLH Instrumentation Schematic Diagram

secondary sodium loop linking the IHX to the steam generator, and a steam/water loop which includes pressure reducers, desuperheaters, a condenser, a feedwater pump, and feedwater heaters.

B. THERMOHYDRAULIC EVALUATION OF ALCO/BLH STEAM GENERATOR

1. Test Results

The SCTI plant performance tests were planned to include a series of steady-state intervals at rated pressure and steam temperature and at 20, 40, 60, 80, and 100% of rated load. These tests duplicated planned test conditions of the steady-state evaluation program. Figure 48 shows the planned test program including the plant performance test phase. Only the 20 and 40% power runs were accomplished up to the time testing was curtailed to permit repair of a tube leak. The 20% results were reported in the previous semiannual report. There were two separate 40% operating periods.

Table 7 presents overall IHX and steam generator system performance for the first 40% power interval. Sodium flowrates were obtained by heat balance with steam parameters. Heat transfer evaluation plans for this phase of testing were limited to comparing the measured sodium temperature profile with theoretical predictions made by UNC. The UNC predictions were based on the original heat transfer equations employed by Alco in design of the steam generator and IHX. UNC made certain corrections in the use of these equations which are incorporated in the predictions. These equations are as follows:

Preheat and Boiling

Convection

$$h_c = 0.019 C_p G_t \left(\frac{G_t D_e}{\mu} \right)^{-0.2} \left(\frac{C_p \mu}{k} \right)^{-2/3} \quad (\text{Colburn})$$

in parallel

Nucleate Boiling (to 100% quality)

$$Q/A = \left[\frac{h_{fg} \mu}{\sqrt{\rho_l - \rho_v} \sigma} \right] \left[\frac{C_p}{0.013 h_{fg} \left(\frac{C_p \mu}{k} \right)^{1.7}} \right]^3 T_x^3 \quad (\text{Rohsenow})$$

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Figure 48. Alco/BLH Steam Generator Steady-State Test Plan

Superheat

$$\frac{hD}{k} = 0.019 R_e^{0.8} P_r^{0.4}$$

(Dittus-Boelter)

Shell-Side (all regions)

$$\frac{hD}{k} = 0.212 (D_e R_e P_r)^{0.6}$$

(Donahoe modified by Tidball)

TABLE 7

STEAM GENERATOR AND IHX SYSTEM PERFORMANCE
AT 40% RATED POWER
(May 15, 1969 - 1530 hours)

Feedwater Flow (lb/hr)	46,930
Feedwater Inlet Temperature (°F)	602
Steam Outlet Temperature (°F)	1,032
Steam Outlet Pressure (psia)	2,200
Secondary Sodium Hot-Leg Temperature (°F)	1,150
Secondary Sodium Cold-Leg Temperature (°F)	755
Primary Sodium Hot-Leg Temperature (°F)	1,152
Primary Sodium Cold-Leg Temperature (°F)	833
Secondary Sodium Flow (lb/hr)	343,900
Primary Sodium Flow (lb/hr)	426,600
Primary/Secondary Flow Ratio	1.24
Thermal Power (Mwt)	12.0
Rated Power (%)	40.0
Primary Sodium ΔT (°F)	319
Secondary Sodium ΔT (°F)	395
IHX Lower Terminal ΔT (°F)	75
IHX Upper Terminal ΔT (°F)	~0
Steam Generator Lower Terminal ΔT (°F)	150

Figure 49 shows the measured and predicted (by UNC) sodium temperature vs area for the 20% power case. (This figure was shown in the previous report.) The profile is obtained from thermocouples 13 through 39 of Figure 48. Saturated vapor and liquid area points are shown as obtained by heat balance. The measured sodium inlet temperature is within a few degrees of that predicted. (The discrepancy between measured and predicted curves in the superheat zone reflects the fact that no predicted points were generated between the saturated vapor and steam outlet points). Figure 50 shows the measured 40% profile and a predicted profile for 60% load. (There was no predicted profile available for 40%.) Figure 50 also includes for comparison the measured and predicted data at 20%.

Table 8 shows a heat transfer breakdown for the 20% and two 40% load cases with results separated into preheat, boiling, and superheat zones. Figure 50 shows that the sodium inlet temperature at 40% load is well above that predicted for a higher load. Table 8 shows that the mean boiling zone heat transfer coefficient is lower at 40% by a factor of about four. Table 8 also shows close agreement between the boiling zone results for the two 40% cases, thus confirming validity of the results. In defining the preheat zone it was necessary to interpolate between adjacent thermocouples to locate the apparent saturated liquid point. The large discrepancy among the preheat heat transfer coefficients of Table 8 probably indicates inaccurate interpolation rather than actual differences.

The data thus indicate a gross discrepancy between performance at 20% and 40% load, with heat transfer at 40% much poorer than predicted.

In the standard operating mode of the SCTI it is possible to compensate for low heat transfer by raising steam generator inlet temperature (IHX outlet temperature) and raising IHX primary inlet temperature accordingly. However, the compensation process reaches its limit when primary sodium temperature reaches the IHX limit of 1200°F. Figure 51 shows predicted IHX and steam generator inlet temperatures vs power level as per the Alco/UNC predictions; it also shows the measured 20 and 40% load data. By extrapolation it may be seen that the 1200°F sodium temperature would be reached at about 45% load. This indicates that unless heat transfer improves greatly at higher loads, many of the planned steady-state tests of Figure 48 cannot be performed at their indicated power level.

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Figure 49. Steam Generator Sodium Temperature Profile
(20% rated power)

7693-5922

Figure 50. Steam Generator Sodium Temperature Profile
(40% rated power and predicted 60% power)

LMEC-70-1

TABLE 8
ALCO/BLH STEAM GENERATOR STEADY-STATE PERFORMANCE

Operating Condition and Time	Section of Steam Generator	Sodium-to-Water ΔT ($^{\circ}F$)		LMTD* ($^{\circ}F$)	Heat Flow (Btu/hr)	Area (ft ²)	Heat Flux (Btu/hr-ft ²)	Heat Transfer Coefficient, U (Btu/hr-ft ² - $^{\circ}F$)
		Hot	Cold					
20% Power (5/14/69) 1330 hr	Preheat	47.5	58	52.6	1.78×10^6	128	13,906	264
	Boiling	245	47.5	120.4	9.60×10^6	180	53,333	443
	Superheat	16	245	82.1	8.47×10^6	975	8,687	106
	Total	-	-	-	19.85×10^6	1283	15,472	183
40% Power (5/15/69) 1530 hr	Preheat	142.5	153	147.7	3.71×10^6	39	95,128	644
	Boiling	338.5	142.5	226.5	20.04×10^6	731	27,415	121
	Superheat	118	338.5	209.2	17.17×10^6	513	33,470	160
	Total	-	-	-	40.92×10^6	1283	31,894	147
Second 40% Power (6/21/69) 0100 hr	Preheat	117.5	127.5	121.5	3.80×10^6	90	42,312	348
	Boiling	317.5	117.5	201.2	19.60×10^6	705	27,776	138
	Superheat	98	317.5	188.2	18.20×10^6	487	37,330	198
	Total	-	-	-	41.6×10^6	1283	32,420	170

*Log Mean Temperature Difference

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Figure 51. Predicted Steam Generator and IHX Temperatures vs Power Level with UNC Corrections

It was therefore decided to go beyond original evaluation plans and analyze the steam generator performance in greater detail. Each area increment between thermocouples was defined as a subregion. Heat flux and heat transfer coefficients were calculated for each subregion. Data for each subregion were also used to calculate local wall superheat (tube inside wall temperature minus saturation temperature). This was accomplished by using estimated values of sodium thermal resistance and tube wall thermal resistance as follows:

$$\text{Wall superheat} = \text{sodium-water } \Delta T - (\text{heat flux}) (\text{wall} + \text{sodium resistance}).$$

Based on Alco/UNC data, a wall resistance of $0.00085 \text{ hr-ft}^2 \text{ } ^\circ\text{F/Btu}$ and a sodium film resistance of $0.00014 \text{ hr-ft}^2 \text{ } ^\circ\text{F/Btu}$ were used.

Figure 52 shows the results of data reduction as plots of heat flux and heat transfer coefficient vs wall superheat. The 20% results exhibit the classical heat flux vs ΔT curve, which includes a rising nucleate boiling region, a maximum point of critical heat flux and a descending region of partial film boiling. The 40% results show little relationship either in magnitude or shape of curve to the 20% results and it is not possible to identify specific regimes of boiling on this curve.

2. Detailed Analysis of Heat Transfer Data

The subregion data were also evaluated in terms of steam quality. In this form the results could be analyzed with reference to experimental data on flow and heat transfer mechanisms in tubes. The method of analysis was: (1) to identify the flow patterns and associated heat transfer mechanisms as a function of quality and other parameters; (2) to apply appropriate heat transfer correlations to each regime; and (3) to compare heat transfer thus calculated with the measured results. The details of this study are described in the following text.

a. Flow Patterns and Heat Transfer Characteristics in the Vaporization Zone of the Water-Steam Tube

The phenomenon under consideration is essentially forced-convection vaporization with net vapor generation, and the flow is single-component two-phase flow with the steam quality increasing with the flow length. The heat transfer mechanism in this type of flow depends mainly on the flow pattern of each local two-phase flow regime.

Figure 52. Alco/BLH Steam Generator Heat Flux (Ref tube ID)
vs Wall Superheat

A literature survey indicates that the flow patterns and heat transfer characteristics in the water/steam tubes under consideration may be represented by those shown in Figure 53. A slightly subcooled water enters the bottom of a vertical tube which is heated externally by the hot liquid sodium. As the subcooled water flows upward, the bulk fluid temperature and the inner tube wall temperature increase as a consequence of heat transferred. When the inner tube wall temperature reaches a certain degree of wall superheat, small vapor bubbles begin to form at the wall even if the water in the core is at a temperature below the saturation temperature corresponding to local (system) pressure. This is so-called bubble nucleation in subcooled boiling and corresponds to point A in Figure 53. Up to this point, the heat transfer is due to single-phase, liquid flow convection and the heat transfer coefficient is more or less constant. As the water passes point A, more vapor bubbles are generated at the wall and entrained in the continuous liquid flow with some of the bubbles condensing in the subcooled liquid. This flow pattern is called bubbly flow; the heat transfer mechanism is essentially that of nucleate boiling and therefore the heat transfer coefficient rises above the liquid phase value. Meanwhile the bulk fluid temperature continues to increase and finally reaches the saturation point. Thereafter bulk boiling takes place with net vapor generation.

As the quality or flow length increases, the individual bubbles agglomerate and grow to form slugs or plugs of vapor, which periodically pass a given point on the wall. This is the slug-flow regime. At higher quality, the vapor flows in a high-velocity core in the center of the tube, and a thin film of liquid covers the tube wall. Liquid droplets are dispersed in the vapor core. This flow pattern of vapor core and liquid annulus is called annular flow. In this flow pattern, both vapor and liquid velocities increase with flow quality because of constant flow area and larger specific volume of vapor state over liquid state. With increased two-phase flow velocity, the wall superheat required for initiation of nucleate boiling increases. In other words, chances of bubble formation on the wall decrease with increasing vapor-induced liquid velocity. This phenomenon is sometimes called "suppression" of nucleate boiling. This changes the character of the heat transfer mechanism, namely, heat is transferred from the hot wall to the liquid in the annulus by forced convection and evaporation takes place at the liquid-vapor interface instead of at the tube wall. The heat transfer coefficient increases with quality as the two-phase flow velocity increases.

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Figure 53. Characteristic Heat Transfer Coefficient Flow Patterns in a Vertically Heated Tube

As the quality increases further, the liquid volume decreases due to vaporization and thus the liquid film in the annulus breaks off the wall, resulting in patches of dry walls, until the whole flow becomes a mist flow in which the vapor state is continuous and the liquid is dispersed in the vapor as droplets. In this transition from annular to mist flow, the heat transfer coefficient decreases due to poor heat transfer characteristic associated with the vapor flow as compared with the liquid flow. In mist flow, heat is transferred from the hot wall to the vapor by forced convection and then from the vapor to the liquid droplets. Once the mist flow is established, the heat transfer coefficient increases with increasing quality because continuous vapor generation accelerates the vapor flow velocity. With all the liquid evaporated, the flow will become entirely vapor-saturated and further heat input will generate superheated steam.

The characteristic curve for the heat transfer coefficient (i. e., Figure 53) moves right with low heat input and moves left with high heat input. This means that the critical point C occurs at high quality for low heat input and at low quality for high heat input.

b. Comparison Between Theory and Experiment

Figure 54 shows the data for 20 and 40% rated powers. Plotted against the flow quality are the heat transfer coefficient and heat flux. For the case of 20% rated power, the shape of the curve for the heat transfer coefficient is similar to that of Figure 53, even though the mist flow regime does not appear to exist, and agreement between experiment and theory is rather good. For the case of 40% rated power, the heat transfer coefficient is much too small compared with that of 20% rated power case, and no theoretical prediction was attempted for this case at this time except for three points to be mentioned later.

To reduce and thus correlate the experimental data, the entire flow, with phase change, is generally divided into three regions: (1) liquid-wetted region I, (2) vapor-covered dry wall region III, and (3) transition from regions I to III as shown in Figure 53. Many experimental data and correlation attempts have been reported for region I. The best correlation may be that of Chen,^{*} which indicates the heat transfer coefficient in region I consists of microconvective

^{*}J. C. Chen, "A Correlation for Boiling Heat Transfer to Saturated Fluids in Convective Flow," ASME Paper No. 63-HT-34 (1963)

Figure 54. Alco/BLH Steam Generator Comparison of Experimental Data with Theoretical Model

and macroconvective coefficients, the former representing nucleate boiling contribution and the latter representing forced convection contribution with weighing factors S and F. For region I:

$$h_{\text{Chen}} = h_{\text{mic}} + h_{\text{mac}} ,$$

where

h_{mac} = macroconvective coefficient

$$= 0.023 \frac{k_L}{D} \left[\frac{GD (1 - x)}{\mu_L} \right]^{0.8} \left(\text{Pr}_L \right)^{0.4} (F)$$

h_{mic} = microconvective coefficient

$$= \frac{0.00122 k_L^{0.79} C_{pL}^{0.45} \rho_L^{0.49} g_c^{0.25} (T_w - T_{\text{sat}})^{0.24} (p_w - p_s)^{0.75} (S)}{\sigma^{0.5} \mu_L^{0.29} H_{fg}^{0.24} \rho_v^{0.24}}$$

$$F = f\left(\frac{1}{X_{tt}}\right)$$

$$S = f(\text{Re}_L, F)$$

X_{tt} = Martinelli two-phase flow parameter.

Chen's correlation was applied to two data points for the case of 20% rated power (shown in Figure 54 as points 2 and 3). Note that point 1 represents the liquid phase heat transfer coefficient.

As for the critical or peak heat flux point (which is point 4 in Figure 54 or point C in Figure 53), the General Electric equation* shown below was used, with the help of the Cichelli-Bonilla curve, for system pressures other than 1000 psia. The results shown in Figure 54 as point 4 is close to the experimental indication. For the critical heat flux point

$$\begin{aligned} \frac{(q/A)_{\text{Critical}} (\text{at } 1000 \text{ psia})}{10^6} &= 0.705 + 0.237 (G/10^6) & x < x_1 \\ &= 1.634 - 0.270 (G/10^6) - 4.71 x & x_1 < x < x_2 \\ &= 0.605 - 0.164 (G/10^6) - 0.653 x & x_2 < x \end{aligned}$$

*E. Janssen and S. Levy, "Burnout Limit Curves for Boiling Water Reactors," General Electric Company Report APED-3892 (1962)

where

$$x_1 = 0.197 - 0.108 (G/10^6)$$

$$x_2 = 0.254 - 0.026 (G/10^6)$$

$$G = \text{mass flowrate (lb/hr-ft}^2\text{)}$$

Experimental data and correlations for the transition region II of Figure 53 are very rare. Application of McDonough-Milich-King* correlation which uses the critical point as a reference, produced point 5 of Figure 54. For region II then,

$$(q/A)_{\text{McDonough}} = (q/A)_{\text{Critical}} - C(T_{W, \text{Transition}} - T_{W, \text{Critical}})$$

where

$$C = 1100 \text{ for } p = 2000 \text{ psia}$$

$$= 1180 \text{ for } p = 1180 \text{ psig}$$

$$= 1500 \text{ for } p = 800 \text{ psia.}$$

Point 6 represents the heat transfer coefficient for the saturated vapor flow.

The data for the 20% rated power do not show region II of Figure 53. However, if such a region exists, the following equation is proposed:

$$h = 0.023 \frac{K_v}{D} \left(\frac{G D x}{\mu_v} \right)^{0.8} \left(Pr_v \right)^{0.4} (C) ,$$

where

$$\frac{G D x}{\mu_v} = \text{Reynolds number for vapor fraction}$$

C = correction factor to be determined by comparison with experimental data.

*J. B. McDonough, W. Milich, and E. C. King, "An Experimental Study of Partial Film Boiling Region with Water at Elevated Pressures in a Round Vertical Tube," A.I.Ch.E. Preprint No. 29, Fourth National Heat Transfer Conference (1960)

This is an ordinary single-phase flow equation, but it uses the Reynolds number for vapor fraction and correction factor which should be determined by comparison with experimental data. Using this type of equation is justified by the fact that in this region, vapor is a continuous flow medium, and liquid is dispersed in the vapor core as droplets; thus, heat flows first from the hot wall to the vapor and then from the vapor to the liquid droplets.

In the case of 40% rated power, three calculation points are shown in Figure 54. Points 7 and 8 are the liquid and vapor phase heat transfer coefficients, respectively, and they are both greater than experimentally indicated water-steam mixture values. Since the data indicate that mist flow may have existed near the quality of 80%, the mist flow heat transfer equation was applied at this quality. Point 9 is the resulting point with the correction factor of unity. When the theory was matched to experimental value, C was calculated to be about 0.4. No further consideration was given to the case of 40% rated power.

This intensive analysis indicates that the 20% results agree well with expected data, but that the 40% results cannot be so correlated.

3. Investigations of Heat Transfer Problem

A program was initiated to investigate the causes of the observed poor and anomalous heat transfer. The avenues of investigation include the following possibilities:

- 1) Film boiling may exist through most of the boiling zone. This may be caused by the fact that a high sodium-water ΔT exists at the vapor end of the boiling zone. This high ΔT is inherent when the unit is operated as a once-through, counterflow evaporator plus superheater. The ΔT may be reduced by operating the unit as an evaporator only. This hypothesis is being investigated experimentally in a diagnostic test described in the following section.
- 2) Static or dynamic stability. The apparent low heat transfer might be the time-averaged result of dynamic instability or the population-averaged result of static instability among tubes. Instability may result in establishing a flow regime associated with poor heat transfer.

- 3) The operating path followed in reaching a steady-state condition may influence the mode of heat transfer. This possibility is also being investigated in the diagnostic test program.
- 4) Surface fouling of the steam tubes.
- 5) Separation at the stainless steel-Inconel interface of the bimetal tubes.

4. Diagnostic Test Program

This program has been planned for four phases of testing. The first phase tests the relation of boiling zone heat transfer to sodium-water ΔT by: (1) operating the steam generator almost entirely as an evaporator with superheat limited to 50°F; (2) varying the sodium inlet-outlet ΔT by varying the flowrate of sodium while maintaining constant energy throughput. The water flowrate, inlet temperature, and steam pressure for this phase are identical to those employed in the 40% plant performance test. However, as the superheat regime is eliminated the actual power is less than 40%.

The second phase investigates the effect of water flowrate on heat transfer. Sodium-water ΔT will be maintained at the lowest value possible at each water flowrate by maintaining sodium flowrate at maximum value and again limiting superheat to 50°F.

The third phase will repeat the 20 to 40% load points of the plant performance tests. However, load will be increased in 5% increments to more closely define the changes in heat transfer between the two levels. Power will then be increased beyond 40% in 5% increments until the 1200°F sodium temperature limit is reached. These tests will thus define the maximum power capability of the unit.

The fourth phase will repeat test points of the first in reverse order. The effects of operating path on heat transfer (if any) would show up as a hysteresis in the combined results of the first and fourth phases.

The test conditions for the four phases of testing are given in Table 9. The final test of the first series of tests also constitutes the first test of the second series.

5. Information Meeting on Steam Generator

A meeting concerning the Alco/BLH steam generator heat transfer problem was held at LMEC on October 24, 1969. Attendees represented seven heat

TABLE 9
DIAGNOSTIC TEST CONDITIONS

Test Number	Feedwater Flow (lb/hr)	Feedwater Inlet Temperature (°F)	Steam Outlet Temperature (°F)	Steam Pressure (psia)	Steam Generator Secondary Sodium ΔT (°F)	Steam Generator Secondary Sodium Inlet Temperature (°F)*	Secondary Sodium Flow (lb/hr)	Primary Sodium ΔT (°F)	Primary Sodium Inlet Temperature (°F)*	Primary Sodium Flow (lb/hr)	Power (mwt)
1	46,900	600	700	2,200	275	900	344,000	207	901	459,000	8.2
2	46,900	600	700	2,200	200	840	465,000	150	842	620,000	8.2
3	46,900	600	700	2,200	150	805	620,000	113	809	826,000	8.2
4	46,900	600	700	2,200	75	760	1,240,000	72	775	1,300,000	8.2
5	78,000	600	700	2,200	125	830	1,240,000	120	853	1,300,000	13.7
6	109,000	600	700	2,200	175	†	1,240,000	168	+31†	1,300,000	19.2
7	140,000	600	700	2,200	225	†	1,240,000	214	+40†	1,300,000	24.7
8	23,400	600	1,050	2,200	400	1,065	170,000	300	1,065	227,000	6.0
9	29,200	600	1,050	2,200	400	1,085	213,000	300	1,085	284,000	7.5
10	35,100	600	1,050	2,200	400	1,105	257,000	300	1,105	343,000	9.0
11	41,000	600	1,050	2,200	400	1,125	300,000	300	1,126	400,000	10.5
12	46,900	600	1,050	2,200	400	1,150	344,000	300	1,151	459,000	12.0
13	52,700	600	1,050	2,200	400	1,175	387,000	300	1,177	516,000	13.5
14	58,600	600	1,050	2,200	400	1,200	430,000	300	1,200	573,000	15.0
15	46,900	600	700	2,200	75	760	1,240,000	72	775	1,300,000	8.2
16	46,900	600	700	2,200	150	805	620,000	113	809	826,000	8.2
17	46,900	600	700	2,200	200	840	465,000	150	842	620,000	8.2
18	46,900	600	700	2,200	275	900	344,000	207	901	459,000	8.2

*Predicted value, not a prescribed test condition.

†Steam generator heat transfer area insufficient to obtain required steam conditions using present heat transfer information.

transfer equipment manufacturers and several AEC national laboratories. The test data, analysis results, and diagnostic test program described above were presented. A number of possible approaches to the problem were recommended including the following.

Maldistribution of steam-side conditions among tubes may be present. This would tend to invalidate the results of detailed analyses based on an assumption of uniform flow. Maldistribution could also be a cause of poor heat transfer. Inlet orificing was recommended as a way of correcting this condition and thus obtaining more valid test data.

A test with the water in liquid state through the entire heat transfer region would be useful in determining the contribution of sodium-side heat transfer, surface fouling, and bimetallic contact resistance.

Most of the possible causes of low heat transfer discussed in this report were suggested in this meeting. The diagnostic test program was generally approved.

6. Boiling Instability Study

A study of boiling instability has been initiated. Initially, it was planned as a distinct task from the basic performance evaluation effort. Its purpose was to determine whether stability tests might be planned as a future special test program. However, in view of the possibility that instability may be a factor in the heat transfer anomalies already observed, this study is now considered to be in the main stream of the efforts being concentrated on the heat transfer problem. The initial phase of this study consisted of a review of published theoretical and experimental studies and the application of their results to the design and operating conditions of the Alco/BLH steam generator. The studies generally fall into two categories: static instability (steady-state tube-to-tube variation of flow), and dynamic instability (timewise variation of flow).

One type of static instability is characterized by a region of negative slope in the pressure drop-flowrate relation. An example of this condition is shown in Figure 55. The significance of this curve is that under a certain range of pressure drops there are three possible flowrates for a given pressure drop. In the region between points A and B, an increase in pressure drop decreases

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Figure 55. Pressure Drop - Flowrate Relationship Under Certain Two-Phase Flow Conditions

the flowrate, which is an opposite phenomenon to a single-phase pressure drop-flowrate relation. In this region of the negative derivative of the pressure drop-flowrate relation, the flow is unstable (Ledinegg stability criterion). For example, the unstable operating point C in Figure 55 moves to either point D or point E where the derivative of the pressure drop-flowrate relation is positive. This means that for a multichannelled flow system, in which a constant pressure drop from the inlet to outlet manifolds is imposed, some channels may take higher (or lower) flowrates than others, resulting in a flow maldistribution. In general, the region of negative slope of the pressure drop-flowrate relation increases with the increasing inlet subcooling and decreases with the increasing system pressure.

The Lieb*-Fraas model[†] a published work based on the above mode of instability was applied to the Alco/BLH system conditions. This model incorporates the following major assumptions:

- 1) Uniform heat input.
- 2) Friction factor does not vary significantly with mass flowrate.
- 3) Homogeneous flow — no velocity slip and no vapor superheat were assumed.
- 4) Momentum pressure drop was not considered in the two-phase flow region.

The model, first developed by Leib and later extended in detail by Fraas, uses the pressure drop in two parts: frictional pressure drops in subcooled regions and in vaporizing regions.

$$\Delta P = \Delta P_{\text{subcooled}} + \Delta P_{\text{vaporization}}$$

$$= 4f \frac{v_f G^2}{2} \frac{L_{sc}}{D} + \int_{L_{sc}}^L 4f \frac{v G^2}{2} \frac{dz}{D} , \quad \dots (1)$$

*E. F. Leib, ASME Trans., 1941, p 344

†A. P. Fraas, "Flow Stability in Heat Transfer Matrices Under Boiling Conditions," USAEC Oak Ridge National Laboratory Report CF-59-11-1 (1959, addition in 1961)

where

- f = friction coefficient
- G = mass flowrate per unit flow area
- v_f = specific volume of saturated liquid
- v = specific volume of two-phase mixture
- z = axial coordinate, measured from tube inlet
- D = tube inner diameter
- L_{sc} = subcooled flow length
- L = total flow length.

Equation 1 is manipulated with use of the above-stated assumptions to yield an expression

$$\Delta P = \frac{16 f L v_f}{\pi^2 D^5} \left[\left(\frac{v_g}{v_f} - 1 \right) \left(\frac{Q}{W H_{fg}} - \frac{\Delta H_{in}}{H_{fg}} \right) \left(1 - \frac{W \Delta H_{in}}{Q} \right) + 2 \right], \quad \dots (2)$$

where

- v_g = specific volume of saturated vapor
- Q = total heat input
- W = weight flowrate
- H_{fg} = latent heat
- ΔH_{in} = inlet subcooling = H_f (enthalpy of saturated liquid)
- H_{in} (inlet subcooled fluid enthalpy).

Equation 2 is used to plot the pressure drop-flowrate curves in dimensionless form (as seen in Figure 55) and the flow in the region with the negative slope is considered unstable.

When the model discussed was applied to Alco/BLH flow/system conditions, the resulting curve for ΔP vs W is as shown in Figure 56. This curve does not reveal a region of negative slope in the ΔP - W relation, and therefore, an unstable flow of the Leib-Fraas type is not expected to occur in the LMEC system.

However, the simplified assumptions of flow regime, pressure drop, and heat flux employed in this model may make it a poor simulation of an actual

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Figure 56. Alco/BLH System as Applied to Leib-Fraas Model

steam generator system. Actual pressure drop may follow different laws for the various flow regimes shown in Figure 56. Heat flux would also be a function of the various flow regimes as well as the local temperature of the heating fluid. Substitution of actual pressure drop and heat transfer conditions for the simplified assumptions of this model may have considerable effect on the result. Therefore, the Leib-Fraas model result is not considered conclusive.

A number of dynamic models of instability were also studied for applicability to the Alco/BLH system. Several of these, which were suitable for rapid calculations, indicated marginal stability or instability. Others require computer programs, which were not immediately available, for their execution. All of these models contain simple flow and heat transfer assumptions which compromise their applicability. No model reviewed to date includes the superheat region, which is inherent in a once-through steam generator.

It is evident, therefore, that theoretical investigation of instability will require a more sophisticated model to study flow instability in a vaporizing system from a more realistic point of view. Consequently, a program has been undertaken for the theoretical simulation of flow instability in the LMEC system in line with the two-phase flow model postulated in Figure 55. The entire water/steam tube will be divided into six regions: (1) single-phase, subcooled liquid region; (2) subcooled boiling region; (3) bulk boiling region with liquid-wetted wall; (4) transition region from wetted to dry wall conditions; (5) bulk boiling region with dry wall; and (6) superheated region. For each of these regions, there will be written basic equations of continuity, momentum, energy, and state, and proper relations for wall-friction, heat transfer, and slip ratio, with proper boundary designations between regions. The model will be computer programmed.

Instrumentation of the steam generator for measurement of maldistribution and instability effects is also being undertaken. It appears feasible to instrument approximately 10 tubes in a pattern which would measure radial, angular, or random differences in flow and temperature. Thermocouples could be placed at the tube outlets so as to measure outlet temperature differences among individual tubes. Flow measuring devices may also be placed at the inlet ends of tubes to measure flow variation of individual tubes. A compact hot wire anemometer has been located which, according to its manufacturer, has successfully

measured flow under conditions similiar to those of the Alco/BLH steam tubes. The device is claimed to have a high frequency response and could thus measure both static and dynamic instability effects.

7. Tube Fouling Study

To observe the effect of fouling on heat transfer in the steam generator, a limiting case was investigated. For the sake of discussion, it has been assumed that fouling exists and is the sole cause for the 40% power run discrepancy. To see how much improvement of the film coefficient is possible, it must be assumed that the worst possible type of fouling exists. Using Kreith, "Heat Transfer," a fouling thermal resistance value was obtained. This value, $0.001 \text{ hr-ft}^2 \text{ - } ^\circ\text{F/Btu}$, is the average fouling resistance for treated boiler water above 125°F . This value of thermal resistance should be much higher than any fouling resistance in the Alco/BLH steam generator, since the purity of the water used in the SCTI is much better than that used commercially. Therefore, this could be considered a worst-case analysis. To generate usable results, the fouling resistance was subtracted from the reciprocal of the film coefficient and this resultant was re-inverted and plotted vs quality. This plot is the dotted line in Figure 57.

As is evident from Figure 57, the only effects that fouling could have are: increasing the negative slope of the film coefficient curve, and increasing the magnitude of the film coefficient. Introducing a fouling resistance of $0.001 \text{ hr-ft}^2 \text{ - } ^\circ\text{F/Btu}$ into the value of the film coefficient does not cause the 40% data to resemble the 20% data. In addition to this, there is a time element to consider. The 40% run was made 24 hr after the 20% run. It is highly unlikely that fouling would have occurred during this time and not during the previous 3 years. Therefore, fouling can be excluded as the sole cause for the divergence between 40% and 20% data.

8. Bimetal Separation

The possibility of bimetal separation, which is associated with the unique design of the Alco/BLH steam tubes, appears difficult to resolve. It is based on the fact that there is no metallurgical bond between the inner Inconel liner and the Type 316 stainless steel outer tube material. An Alco test report indicates that the two materials have a fairly strong shear bond. A complete

Figure 57. Alco/BLH Steam Generator Steam-Side Heat Transfer Coefficient vs Steam Quality Corrected for Maximum Fouling

separation over a large area has been calculated to reduce heat transfer far below the 40% data. Partial separation, producing a less drastic effect, would be hard to disprove. However, there appears to be no sound reason why there should be good contact at 20% and separation at 40%.

9. IHX Temperature Profile and Performance

The IHX primary (shell-side) sodium temperatures are measured with a series of internal thermocouples located at 16 different levels along the sodium flow path. The relative locations of the thermocouples (with their identification numbers) are shown in Figure 47.

The primary sodium temperature profile through the IHX for the 40% power level (May 15, 1969) operation is shown in Figure 58. The overall heat transfer coefficient (U) was calculated for the cross-flow and cross-axial-flow regions, using the Alco total tube area of 1340 ft². Sufficient temperature differences did not exist in the axial region to allow a meaningful calculation. The cross-flow region constitutes 63% of the heat transfer area, and the cross-axial region constitutes 15%; the two comprise 78% of the total IHX heat transfer area. The resulting overall heat transfer coefficients were:

1) Cross-flow — 1533 Btu/ft²-hr-°F.

2) Cross-axial-flow — 1324 Btu/ft²-hr-°F.

The curve drawn in Figure 58 was predicted, using the calculated value of U in the cross-flow region. The heat transfer coefficient between the inlet and first donut baffle was 967 Btu/ft²-hr-°F.

C. STRUCTURAL EVALUATION

1. Tube Sheet Temperatures

Anomalous temperatures in the region of the steam generator lower tube sheet at a plant heatup condition were reported in the previous semiannual report, and were previously reported by UNC in test data from 1966. The 40% power level conditions included a high fluid temperature difference across the lower tube sheet of the steam generator. As the anomaly is concerned with the manner in which this temperature difference is divided between the thermal shield and the tube sheet itself, the high ΔT served to provide good definition of the temperatures in question.

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Figure 58. IHX Primary Sodium Temperature Profile
 (May 15, 1969; 1530 hours; 40% power)

The steam generator temperature data from the 40% power steady-state operations were analyzed using a finite element Thermal Analyzer Program (TAP) model to define the temperature distribution in and around the lower tube sheet. The water inlet temperature was used for dome and tube boundary conditions. The secondary sodium outlet temperature was used on the outboard side of the tube bundle, and the No. 12 internal thermocouple reading was used inboard of the tube bundle. The resulting predicted and measured temperatures in the lower tube sheet region are shown in Figure 59 for the 40% power level.

The results at this condition support the following conclusions (stated in the previous semiannual report:

- 1) The tube sheet upper surface temperatures outside of the tube bundle region are close to or below the predicted value and significantly below the sodium temperature.
- 2) The measured temperatures in the tube bundle area are well above predicted values and of the same order as the temperatures outside of the tube region.
- 3) The thermal shield, therefore, provides the intended isolation of the tube sheet surface from the sodium temperatures, but the tubes do not supply the predicted cooling to the tube sheet in the tube bundle region.
- 4) An asymmetrical temperature distribution around the outside of the shell exists near the lower tube sheet. The measured temperatures in the region of the liner drain slot are generally higher than the values measured 120 degrees away from the drain slot. However, this is not the case on the upper surface of the lower tube sheet, where the temperatures measured nearer the drain slot, outside of the tube bundle region, are lower than those measured 180 degrees away from the drain slot.
- 5) The measured external shell temperatures are well below the predicted values. The TAP model may need revision in this area.

Figure 59. Steam Generator Lower Tube Sheet Temperature Data
(40% power; May 15, 1969; 1530 hours)

2. Gas Blanket Temperature Distribution

The 40% load point included a 100°F ΔT between inlet sodium and outlet steam temperature. It thus provided a good opportunity to test the effectiveness of the gas blanket which is intended to provide thermal protection for the upper tube sheet. The temperature profile in the gas blanket was plotted as shown in Figure 60; for most of its length the gas space is in thermal equilibrium with the steam outlet temperature, as was intended. The temperature rises toward the sodium inlet temperature at the apparent sodium level.

3. Tube Leak and Manway Crack Investigation

A tube leak and accompanying sodium-water reaction were discovered on June 22, 1969, necessitating shutdown of the steam generator for leak location and repair (refer to Section II for further details). The resulting inspection activities disclosed the presence of a second defect, a series of cracks in the seal groove of the manway.

The tube leak was located near the upper end of a tube, slightly above the point where the tube passes within the tube sheet hold. It proved impossible to remove the tube for metallurgical examination; without such an examination the conclusive cause of the leak could not be determined. However, it was possible to calculate whether failure was likely to have resulted from the pressure and temperature to which the point of failure was exposed while the steam generator was operating; therefore, a temperature history was compiled. The steam tubes are of bimetallic construction with an inner liner of Inconel and an outer tube of Type 316 stainless steel. The difference in coefficient of expansion between the two materials could lead to a mode of thermal fatigue when the tubes are temperature-cycled. Calculations of thermal fatigue based on the temperature history indicated that the accumulated cycle life was less than 1% of the estimated cycle life to failure. It is thus concluded that failure was unlikely to have been caused by thermal fatigue alone; some mode of corrosion and/or an original manufacturing defect must have been a contributing factor.

A probable cause of manway cracking was identified. It was discovered that in addition to the cracks, the manway lip had been deformed under the seating force of the sealing ring. A metallurgical "boat" sample including a section of

Figure 60. Steam Generator Sodium-Argon Interface
(40% rated power; May 15, 1969; 1530 hours)

a crack was removed from the defect area. Microexamination of this sample revealed the presence of subsurface microfissures at the interface of the stainless-steel parent material and the Inconel overlay. The crack originated at one of these microfissures (refer to Section II for details).

The thickness of material between the seal groove and the manway inner diameter was far thinner than prescribed in standard designs for this type of seal. The improper design is apparently a compromise forced upon Alco by the problem of scaling down an original 70-Mwt steam generator design to a 30-Mwt test model. If proper ring seal dimensions had been used the manway flange would have been so large as to preclude any structural similarity to the full-scale steam header. It is surmised that the application of bolt torque to seat the sealing ring created excessive stress in the inadequate design. The stress concentration caused by the microfissures then resulted in cracks.

A topical report describing tube leak detection, location, causes, and repair is being published. A report covering the manway failure is in preparation.

4. Alco/BLH Steam Generator Thermal History

The Alco/BLH steam generator was installed in the SCTI in 1965. However, steaming (outlet steam temperature greater than 450°F) conditions have only been reached during 1966 and 1969, primarily because of the steam line repair (2 years of inactive time). Therefore, investigation of the thermal history of the unit was restricted to these two years. Since the thermal history was prepared to support the study of the tube failure, emphasis was placed on the steam outlet temperature as the primary parameter. Only the steam temperature changes are indicative of changes in the tube temperature at the point of failure (near the argon side of the upper tube sheet). Sodium temperatures will not affect tube temperatures in the failed region because of the insulating properties of the argon gas blanket at the top of the steam generator.

The steam temperature was taken from recorder TR-102 (multipoint) for the 1969 data, and from the control room data logs (which are recorded at two-hour intervals) using TRC-200 (circular chart) for the 1966 data. The sodium (inlet and outlet) and water terminal temperatures were also plotted for 1969 operation for other purposes which required the use of TR-102. However, these

parameters were not investigated for the 1966 data; therefore, to expedite the collection of information, the control room data logs were employed.

A summary of the number of operating hours at various steam temperatures is presented in bar-chart form in Figure 61. The temperature intervals are arbitrarily divided into 100°F increments, and the total operating time for 1966 and 1969 and a summation of the time for both years are shown. The operating time shown for the 400 to 500°F interval represents other isothermal operation with water in the system, or an early increase in sodium temperature. The unit has been operated for a total of approximately 2300 hr, of which 40% of the time was logged at steam temperatures below 500°F and only 7% at temperatures greater than 900°F.

A second important facet in the investigation of the tube failure is the number of temperature changes (thermal cycles) that the unit has experienced. A summary of the steam temperature changes is included in Table 10. A change in temperature level was recorded each time the temperature of the system changed between two 100°F intervals and then remained within the second 100°F interval for at least 4 hr.

The steam generator was started up 17 times in 1966 and 10 times in 1969. The startup procedure consists of either:

- 1) Heating the unit to 400°F with sodium circulating in the system; then introducing feedwater at a temperature within 50°F of the sodium; or
- 2) Heating the steam generator tubes by circulating feedwater at about 400°F and then introducing sodium at about 400°F into the system.

The second startup method was employed only when the secondary sodium system had been drained. The startup method is not important as regards gradients in the tubes in the region of failure, since both methods will slowly heat the tube to 400°F. A total of 44 different increases in temperature were noted for the unit. These temperature increases are divided into various intervals and presented in Table 10.

The steam generator has been shut down a total of 27 times. However, 22 of these shutdowns were initiated with steam temperatures below 900°F, which introduces less severe gradients in the system. Only five shutdowns have been performed with the steam temperature above 900°F, which could introduce significant transients in the system.

Figure 61. Alco/BLH Steam Generator Steam Temperature History

TABLE 10

SUMMARY OF STEAM TEMPERATURE CHANGES IN ALCO/BLH
STEAM GENERATOR

Type of Change	Number of Changes		
	1965 to 1966	1969	Total
Startups:	17	10	27
Temperature Increases (°F):			
400+ to 500+	7	2	9
400+ to 600+	9	2	11
400+ to 700+	0	4	4
400+ to 800+	0	2	2
500+ to 600+	1	0	1
500+ to 700+	1	1	2
500+ to 800+	1	0	1
600+ to 700+	3	0	3
700+ to 800+	2	3	5
800+ to 900+	1	1	2
800+ to 1000+	0	3	3
900+ to 1000+	1	0	1
Shutdowns			
(From Following Temperature Intervals, °F):			
$T_{\text{steam}} < 600$	5	1	6
$600 \leq T_{\text{steam}} \leq 900$	11	5	16
$900 < T_{\text{steam}}$	1	4	5

5. Strain Gage Interface Problems

Installation of strain gages on the steam generator will begin immediately after the diagnostic tests are completed. A review of calibration and installation requirements indicates that there are many interfaces between strain gage problems, SCTI test conditions, and evaluation objectives. Work has started on resolving these interface problems with the objective of achieving the best possible utilization of strain gages. Specifically the following tasks must be completed:

- 1) Determine the level of strain gage accuracy required for verification of theoretical estimates of stress.
- 2) Formulate operating conditions to provide strain cycling required for stabilization of gages after installation.
- 3) Determine special operating conditions which may be desirable to provide in-place calibration of gages. Errors which could result from removal of gages from the calibration device and reinstallation in the field may thus be reduced.
- 4) Reach a decision on the value of designing special tests to provide data on structural characteristics. The present steam generator steady-state test program has been designed mainly around thermo-hydraulic evaluation objectives and may not provide optimum variation of parameters which influence strain.

VII. INSTRUMENTATION TESTING, EVALUATION, CALIBRATION, AND STANDARDS

(LMFBR Program Plan 4-1.4, 4-3.2, 4-3.6, 4-3.7, 4-3.8, 4-7.3, 4-7.6,
4-7.7, 4-7.8, 4-7.10, 4-7.11)

J. W. McCarty

A. INTRODUCTION

The overall objectives of this project are: (1) to establish capabilities and facilities to develop and maintain standards of measurement; (2) to provide fixed, reproducible, known, sodium environmental conditions; and (3) to perform evaluation testing, calibration, and standardization functions for instrumentation used in the LMFBR program. Included in this project will be the determination of testing and test facilities requirements, modification of existing LMEC facilities, and design of new installations for testing and calibrating instrumentation developed under the LMFBR program. The project will also provide for testing and evaluation of commercial instrumentation and for their adaption for use in 1200°F sodium applications.

The basis for all planning and testing of instrumentation is Section 4 of the LMFBR Program Plan.

B. HIGH-TEMPERATURE STRAIN GAGE MEASUREMENTS

1. Tests ST13, ST14, and ST18

During the previous reporting period a series of tests were performed to evaluate calibration, removal, and reinstallation techniques for the Microdot Model SG420 strain gage. The data analysis has been completed and is presented in the following text.

Once a gage has been calibrated, it is expected that its removal and reinstallation on a test article will produce some change in original calibration. Although it is not necessary for the gage to repeat its original calibration exactly, the change must be predictable within the accuracy requirements of the user. However, in preliminary tests at LMEC with the SG420 gages, significant variations in gage calibration occurred after removal from the calibration bar and subsequent reinstallation on the same bar. The changes were believed to be caused by gage deformation produced during the chiseling

operation used in gage removal, and by the residual strains induced by spot-welding during reinstallation. The most significant change was in apparent strain; although always in the positive direction, it was not predictable in magnitude. Of secondary importance was the zero shift which occurred during the first load cycle at 900°F, which would diminish to a negligible value after two or three load cycles. An intermediate shim was used with the SG420 strain gage to provide a strain transfer medium with sufficient area to allow removal of the gage without damage, and permit leadwire attachment to the gage shanks.

As a result of the preliminary testing, it was decided that a variety of removal and reinstallation techniques be investigated to identify those which would minimize distortion of the gage and shim. A method of determining the effects of the reinstallation on gage calibration was also sought.

The removal methods studied were: laser beam cutting, electron beam cutting, electric discharge cutting, air-abrasive cutting, mill-sawing, and a modification of the original chiseling technique.

Both the laser and electron beam cutting failed to separate the shim from the calibration bar because the material rewelded at the edges of the cut.

The electric discharge cutting technique proved impractical because the cutting fluid required for the tool contaminated the gage and lead wire.

The air-abrasive system, which uses a stream of abrasive particles in air directed at the area to be cut, provided excellent cuts of the shim with no heating or distortion. The one problem was that damage to the calibration bar occurred where cuts intersected, due to abrasive particles striking the bar through the first cut. This could be solved by using an x-y feed table so that a continuous cut, curving at the corners, could be made.

Mill-sawing with standard machine shop techniques cut the shim free of the calibration bar with negligible distortion and no damage to the bar.

Chiseling was done with a single-edge razor blade. The blade was held at a slight angle to the surface of the bar and approximately 45 degrees to the weld line and struck lightly with a small machinist's hammer. Care was taken not to allow the blade to penetrate past the weld line, thus minimizing the distortion of the shim in the area between the weld line and the gage.

As part of this series of tests, the welding techniques used to attach the gage to the intermediate shim and the gage-shim assembly to the calibration bar were investigated.

a. Test Procedure and Description

The gages were tested for apparent strain due to temperature and gage factor characteristics as installed on the calibration bar, after removal from it, and after reinstallation on the same calibration bar.

The gages were first spotwelded to the intermediate shim using the weld pattern shown in Figure 62. The leads were attached, and the assembly was then spotwelded to the calibration bar. The calibration bar is 1/4 by 1 by 12-in. cold-rolled, annealed, Type 316 stainless steel.

Apparent strain cycles were performed using a Marshall Model 2099A electric resistance heating furnace with a heating rate of approximately 35°F/min. The gages were in an unstrained condition during these tests.

Gage factor was determined by measuring the strain output of the gage with the calibration bar loaded in bending. Loads were applied using the constant moment deflection fixture. Maximum strain was approximately 735 micro-strain on the surface of the calibration bar. Corrections for bar thickness, shim thickness, and nominal location of the strain element in the strain tube were used to determine the actual gage factor. All tests were made using the R_{TC} resistor value used during the apparent strain tests.

The three tests discussed in this report are labeled ST13, ST14, and ST18. Tests ST13 (chisel technique) and ST14 (mill-saw technique) were made to compare the two cutting techniques. Twelve gages were selected for each test. One gage failed during the ST13 test; therefore, all statistics are based on a sample size of 11 for ST13 and 12 for ST14.

The following procedure was used for these two tests:

- 1) Spotweld gages to standard intermediate shim (2-1/4 by 1/2 by 0.006-in., see Figure 62).
- 2) Spotweld two gage-shim assemblies to each calibration bar (1/4 by 1 by 12 in., Type 316 stainless steel), welding 1/16 in. from edge of shim on long side and one row across each end. Use 10 welds per inch. Locate gages on centerline of bar.

GAGE TO INTERMEDIATE SHIM

FIRST INSTALLATION

SECOND INSTALLATION

++++++ WELDS

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Figure 62. Weld Pattern

- 3) Stabilize gages at 1100°F for 16 hr.
- 4) Select optimum value of R_{TC} by temperature cycling from room temperature to 900°F (to minimize apparent strain to within $\pm 20 \mu\epsilon$ at 900°F).
- 5) Record two apparent strain cycles from room temperature to 900°F and return to room temperature.
- 6) Record three load cycles at room temperature.
- 7) Record three temperature cycles from 350 to 900°F.
- 8) Record three load cycles at 900°F.
- 9) Record one apparent strain cycle from room temperature to 900°F and return to room temperature.
- 10) Record three load cycles at 900°F.
- 11) Remove 12 gage assemblies using chiseling technique.
- 12) Remove 12 gage assemblies using mill-sawing technique.
- 13) Reinstall gage assemblies on same bar, spotwelding adjacent to strain flange of gage.
- 14) Record three apparent strain cycles from 350 to 900°F.
- 15) Record three load cycles at 900°F.
- 16) Record one apparent strain cycle from room temperature to 900°F and return to room temperature.

Test ST18 was done to determine the effects of changing the weld technique. Peel tests were performed using the Microdot Rollectrode welder; it failed to produce adequate or consistent welds. A Unitek Model 5-014-01 hand probe was used for additional peel tests. This probe discharges only when a preset force is reached. The welds obtained were consistent and structurally sound, as evident from the holes in the shim material under the peel test.

Ten gages were used for the ST18 series; they were welded to the shim and the shim to the calibration bar using the same weld pattern as the ST13 and ST14 tests but with the Unitek hand probe. The test procedure used for the ST13

and ST14 tests was repeated except that step 7 was eliminated because it did not accurately simulate the actual calibration procedure. An additional load cycle was also added, following the last apparent strain cycle on the second installation, to check gage stability.

b. Test Results

Results of the three tests are summarized in Tables 11 through 15.

A 95% confidence range is shown; that is, there is a 95% probability that the mean or average value of any other group of gages tested from the same lot would fall within the range shown. Small sampling theory was used since the number of samples was less than 30. The confidence range is shown rather than the upper and lower limits to provide the reader an easier interpretation of the scatter associated with each test. The limits may be established by adding and subtracting the range to the average value.

The confidence range was calculated as follows:

$$\text{Range} = \pm t \sqrt{\frac{\sum \bar{X}_i^2 - (\bar{X})^2}{n - 1}}$$

where

$\bar{X}_i^2$ = value of the gage characteristic

$\bar{X}$ = arithmetic mean of the gage characteristic

t = confidence coefficient for 95% confidence level and number of samples used.

The tolerance range given in the tables is that for a 95% probability that 90% of the gages (gage characteristic in question) will fall within this range. This is the most important statistic for these tests in that it establishes the limits within which any one gage tested would be expected to fall.

The tolerance range was calculated as follows:

$$\text{Range} = \pm K \sqrt{\frac{\sum (X_i - \bar{X})^2}{n - 1}}$$

where

X_i = value of gage characteristic

$\bar{X}$ = arithmetic mean of gage characteristic of the gages sampled

K = tolerance factor for 95% - 90% limits.

The test results are discussed primarily with regard to gage characteristics rather than on an individual test basis. The test results are only meaningful in a comparative analysis.

(1) Zero-Shift (Table 11). As reported in earlier tests, it was noted that on the first load cycle the indicated strain output of the gage at maximum strain was low, and when the load was removed it became negative. At room temperature, this first cycle zero shift is negligible, usually 10 $\mu\epsilon$ or less. At 900°F this shift becomes quite significant when compared to the expected strain levels for stainless steel. Figure 63 is a typical example of this phenomenon.

TABLE 11
ZERO SHIFT

Test No.	First Installation			Second Installation		
	Avg	CR	TR	Avg	CR	TR
ST13 (chiseled)	-85	±10.2	±41.9	-163	±30.5	±124.8
ST14 (mill-sawed)	-82	±13.1	±53.6	-116	± 8.7	± 37.2
ST18 (chiseled/weld)	-98	± 9.2	±34.2	-125	±14	± 56.2

CR = confidence range ($\mu\text{in./in.}$)

TR = tolerance range ($\mu\text{in./in.}$)

Avg = average zero shift ($\mu\text{in./in.}$)

A comparison of tests ST13 and ST14 indicates an approximate 40% decrease in magnitude of the zero shift for ST14 (mill-saw specimens) over that of the ST13 (chiseled specimens). The scatter in the data as indicated by the confidence and tolerance range is also reduced for the ST14 test.

Figure 63. Intermediate Shim 900°F Load Tests
(Specimen No. ST14B)

The ST18 test data show a decrease in magnitude of zero shift of 30% over that of the ST13 test but is only 8% greater than the ST14 tests. The degree of deformation of the shims for the ST13 and ST18 test specimens was the same.

(2) Apparent Strain. The apparent strain characteristic of the gage changes from the first installation (calibration) to the second installation and is affected by both temperature cycling and load cycling. The maximum deviation in the apparent strain curve occurs at approximately 500°F, as shown in Figure 64 which is a typical apparent strain curve. To simplify the analysis, only the data points at 500°F were corrected for gage factor and presented in Tables 12 through 14.

The effect of temperature cycling on apparent strain is shown in Table 12. The data were obtained from step 14 of the procedure to evaluate the effect of post-installation temperature cycling of the gages. In all cases the shift in the average apparent strain is in the negative direction as a result of the temperature cycling. The average shift in apparent strain for tests ST13 and ST14 is not significantly different but the decrease in scatter for test ST14 is quite evident (a reduction in the tolerance range from ± 241 to ± 39 microstrain).

TABLE 12
EFFECTS OF TEMPERATURE CYCLING ON APPARENT STRAIN

Test No.	First Apparent Strain Cycle			Third Apparent Strain Cycle			Change from First to Third Cycle		
	Avg	CR	TR	Avg	CR	TR	Avg	CR	TR
ST13 (chiseled)	-131	108.5	442.0	-232	115.6	471.0	-78	59.0	240.9
ST14 (mill-sawed)	-240	63.2	263.4	-311	60.3	252.0	-71	9.2	39.0
ST18 (chiseled/weld)	-219	58.5	232.2	-270	63.5	252.4	-51	21.2	84.0

CR = confidence range (μ in./in.)
 TR = tolerance range (μ in./in.)
 Avg = average apparent strain (μ in./in.)

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Figure 64. Intermediate Shim Apparent Strain Due to Temperature
(Specimen No. ST14C-1)

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The average shift in apparent strain for test ST18 was decreased by more than 50% from that of ST13, and approximately 40% from that of ST14.

The effect on apparent strain by load cycling the gage at 900°F is shown in Table 13. The data were obtained from step 16 of the test, which is the apparent strain cycle after the gage has been load cycled three times at 900°F on the second installation. In this case all the average values of apparent strain

TABLE 13
EFFECTS OF LOAD CYCLING ON APPARENT STRAIN

Test No.	Preload			Post-Load			Change from Pre-load to Post-Load		
	Avg	CR	TR	Avg	CR	TR	Avg	CR	TR
ST13 (chiseled)	-232	±115.6	±471.0	-144	±108.5	±449.1	88	±35.0	±145.6
ST14 (mill-sawed)	-311	± 60.3	±251.2	-232	± 67.8	±283.6	79	±18.9	± 79.1
ST18 (chiseled/weld)	-270	± 63.5	±252.4	-189	± 67.1	±266.6	81	±19.7	± 78.1

CR = confidence range (μ in./in.)

TR = tolerance range (μ in./in.)

Avg = average apparent strain (μ in./in.)

shifted in the positive direction. The average values for the three tests are quite similar but the scatter of the ST14 and ST18 data is reduced to approximately half that of the ST13 data.

The average shift from the first to second application for the three tests are quite different, but the tolerance limits of ST14 are contained within the tolerance limits of ST13 (+335 to -101 microstrain for ST14 are within the limits of +408 to -240 for ST13) and the tolerance limits for ST18 almost fit within the limits for ST14 (+355 to +50 microstrain for ST18).

Of primary importance in the discussion of predictability of apparent strain is the tolerance range. As stated earlier, there should be a 95% probability that 90% of the gages (gage characteristic) will fall within this range. This then gives the limits within which any additional gage (from the same lot) could be expected to fall.

The strain gages installed on the Alco/BLH steam generator and IHX will be temperature and load cycled prior to the acquisition of test data. To determine the predictability of the apparent strain from calibration to installation, the apparent strain from step 9 of the procedure (after load calibration) is compared to the apparent strain from step 16 (after three temperature cycles and three load cycles) in Table 14.

The change in apparent strain from the first to second installation incorporates the positive shift due to the reinstallation, the negative shift from temperature cycling, and the positive shift due to load cycling, resulting in a net positive shift in all cases.

The tolerance limits for the ST14 test are reduced by 48% from those of the ST13 test. The tolerance limits for the ST18 are reduced to less than half of the limits of the ST13 tests and 43% from those of the ST14 tests.

TABLE 14
PREDICTABILITY OF APPARENT STRAIN

Test No.	First Installation			Second Installation			Change from First to Second Installation		
	Avg	CR	TR	Avg	CR	TR	Avg	CR	TR
ST 13 (chiseled)	-236	±84.1	±334.2	-144	±108.5	±449.1	84	±81.6	±323.9
ST 14 (mill-sawed)	-349	±57.0	±237.9	-232	± 67.8	±283.6	117	±52.4	±218.5
ST 18 (chiseled/weld)	-393	±77.3	±307.5	-189	± 67.1	±266.6	202	±38.6	±153.3

CR = confidence range (μ in./in.)

TR = tolerance range (μ in./in.)

Avg = average value of apparent strain (μ in. /in.)

(3) Gage Factor (Table 15). As with apparent strain the most important element of the gage factor analysis is the predictability of the change in gage factor from the time the gage is calibrated until it is installed on the test article.

TABLE 15
GAGE FACTOR AT 900°F

Test No.	First Installation			Second Installation			Change from First to Second Installation		
	Avg	CR	TR	Avg	CR	TR	Avg	CR	TR
ST13 (chiseled)	3.141	±0.114	±0.465	3.309	±0.082	±0.337	5.6	±4.4	±18.1
ST14 (mill-sawed)	3.281	±0.123	±0.489	3.591	±0.114	±0.473	8.8	±2.6	±10.6
ST18 (chiseled/weld)	3.220	±0.102	±0.406	3.470	±0.081	±0.324	7.4	±2.02	± 8.0

CR = confidence range (%)

TR = tolerance range (%)

Avg = average change in gage factor (%)

The average change in gage factor, the confidence range, and the tolerance limits are shown in percentages rather than units of gage factor since the errors associated will be a function of the strain level being measured.

The third load cycle was used in all cases to eliminate the nonlinearity and zero shift noted on the first load cycle.

The lower average change in gage factor shown in Table 15 for the ST13 tests is primarily due to the large scatter in the data. The tolerance range was reduced from 18.1% to 10.6% for the ST14 test as compared to the ST13 test. A further reduction of 2.6% was indicated by the ST18 test.

c. Conclusions

Of the removal techniques studied it is apparent that the mill-saw technique is the best. The improvement in all gage characteristics is attributed to the fact that the intermediate shim has less distortion and allowed a better quality and more consistent weld bond of the intermediate shim to the test specimen.

The improvement of the gage characteristics gained from changing the weld technique is obvious even though there was some distortion of the intermediate shim due to chiseling.

The actual improvement due to the new weld technique cannot be determined from this test series. There remains the question of whether the new weld technique in conjunction with mill-saw removal would produce a significant improvement over that of the ST18 test.

As a result of this series of tests, several improvements in technique were formulated and are being developed to increase the predictability of gage characteristics following final installation. This activity is reported later in this section under the discussions on tests ST27, ST28, and ST29.

2. Test ST19

This was the first in a series of tests performed in the strain gage laboratory during this reporting period. Although 15 tests were performed, only the major tests are reported here. The intermediate tests were to develop techniques which were in turn, used in the major tests. These tests were initiated as part of an effort to finalize the calibration and installation techniques to be used with the Microdot weldable strain gages on the SCTI steam generator and IHX. Test ST19 used the Microdot Model SG425 strain gage to: (1) determine its operating characteristics (i. e., gage factor, apparent strain, and drift) in the temperature range from room temperature to 900°F; and (2) determine the feasibility of using these gages on a test article with no previous individual gage calibration. A statistical sample of the gages from the lot to be used would be calibrated and these data, with the correct statistical interpretation, would be used in the reduction of data from the test article.

The SG425 strain gage is similar to the Model SG420. The primary difference is the addition of an integral sheathed lead. The gage elements, both active and temperature compensating, are the same as those of the SG420, being formed of 0.0007-in.-diameter platinum/tungsten alloy. The sheathed lead consists of three 28-gage conductors enclosed in a 0.063-in.-diameter stainless steel tube and insulated from each other and the tube with magnesium oxide. The junction of the lead wires to the gage shanks is done by inert-gas arc welding. The junction is enclosed by a 0.16-in.-diameter sleeve which provides the attachment of sheath to the extension of the gage strain tube. The space within the junction is evacuated and back filled with argon gas to prevent oxidation. Each gage was hydrotested at 500°F and 2500 psig.

a. Test Procedure and Description

Sixteen SG425 gages were selected at random from a lot of 64 gages; they had all been stabilized at 1100°F for 16 hr by Microdot as directed by LMEC. The gages were spotwelded to polished 1/4 by 1 by 12-in. Type 316 stainless steel calibration bars. No intermediate shim was used during these tests.

The following test sequence was used:

- 1) Spotweld two gages to each calibration bar using Unitek Model 5-014-01 force probe.
- 2) Using manufacturer's R_{TC} resistance value, temperature cycle each gage to 900°F to verify that this value limits apparent strain to less than 100 microstrain at 900°F. If not, adjust R_{TC} value until apparent strain at 900°F is less than 100 microstrain.
- 3) Record two apparent strain cycles from room temperature to 900°F and return to room temperature at a heating rate not to exceed 35°F/min.
- 4) Record three load cycles at room temperature. (Maximum load produces approximately 735 microstrain at all temperatures.)
- 5) Record three load cycles at 350°F.
- 6) Record three load cycles at 625°F.
- 7) Record three load cycles at 900°F.
- 8) Record one apparent strain cycle from room temperature to 900°F and return to room temperature.
- 9) Record drift of each gage at 900°F for 500 hr.

During the test, a problem developed with the junction of low-temperature wire with the integral sheath on one gage; so this gage was eliminated from the test. The active element opened at 900°F gage factor, are based on a sample size of 15.

b. Test Results

(1) Gage Factor. The average gage factor of the 15 gages with the 95% confidence limit, and the 95 to 90% tolerance limits is shown in Figure 65 plotted

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Figure 65. SG425 Strain Gage Factor vs Temperature

against temperature. The tolerance band is the most significant statistic when considering individual gage data since this defines the limits within which one has a 95% confidence that 90% of all gage characteristics will occur. The tolerance band shown contained all data points from this test series. The tolerance band is a maximum of 21.4% at 350°F and a minimum of 18% at 900°F.

(2) Apparent Strain. Figure 66 shows the average apparent strain due to temperature, and the confidence and tolerance limits versus temperature. The maximum scatter in apparent strain as indicated by the tolerance limits occurred at 400°F. The tolerance range at this temperature was ± 279 microstrain. The tolerance range at 500°F was ± 270 microstrain, which compares quite closely to the tolerance range of ± 267 reported for the SG420 strain gage.

(3) Drift and Drift Rate. Figure 67 shows the average drift, the confidence limits, and the tolerance range versus elapsed time. The average drift at the end of the 500-hr test was 45 microstrain with a tolerance range of ± 189 microstrain. Of the fifteen gages tested, 9 had a total drift of less than 50 microstrain at the end of the 500-hr period.

Figure 68 shows the average drift rate, the confidence limits, and the tolerance limits versus elapsed time. The drift rate was computed on an individual gage basis using the drift divided by each time increment. The drift rates obtained were then averaged and the statistical parameters calculated. The maximum average drift rate did not exceed 0.65 microstrain/hr and had stabilized to less than 0.05 microstrain/hr at the completion of the 500-hr test. The tolerance limits at the completion of 500 hr was less than 0.5 microstrain/hr.

c. Conclusions

The characteristics of the Microdot SG425 strain gage are very similar to those of the Model SG420; apparent strain appears to be the same,* gage factor is approximately 9% less than the SG420 gages tested on intermediate shims (test ST18), and the drift appears to be approximately the same.

The large scatter in apparent strain prevents the use of a small statistical sample approach to gage calibration. At present, each gage must be pre-calibrated prior to installation on a test article. It is possible, however, that

*M. M. Lemcoe, "Characteristics of Two Commercial High-Temperature Electric Resistance Strain Gages," NAA-SR-12118 (August 15, 1967)

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Figure 66. SG425 Strain Gage Apparent Strain vs Temperature
(Optimized R_{TC})

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Figure 67. SG425 Strain Gage Drift vs Time

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Figure 68. SG425 Drift Rate vs Time

a technique might be developed whereby the gage is calibrated for apparent strain in an unmounted condition (not welded to a calibration bar) and the change in apparent strain from unmounted to installed condition determined from a statistical sample test.

In general, the SG425 gage appears to be the most promising high-temperature strain gage tested to date. The integral lead assembly is a very significant improvement on the SG420 gage and eliminates many manhours of delicate work in the preparation of the gage leadwire assembly for calibration and installation. The probability of damage during handling and installation is much less for the SG425. No protective covers need be fabricated and installed, thus saving additional time on installation. The integral lead provides complete shielding and automatic grounding of the shield, which reduces noise pickup in the strain gage signal. The Fiberglas-insulated, high-temperature leadwire used with the SG420 is more susceptible to damage during installation of insulation on a test article than the integral lead. The reliability of the leadwire junction is greatly improved using the SG425. The only problem found with the SG425 gages tested was with the special junction of low-temperature wire to the integral lead provided by Microdot. The junction was made with individual 22-gage wires, and two intermittent connections were found. Microdot has recommended changing to a cable rather than the individual wires.

Continuing effort in this testing will include a study of the feasibility of calibrating the SG425 gage on an individual basis with no intermediate shim. The gage will be welded to the calibration bar along the outer edge of the strain flange, then sheared inside this weld line for removal. Microdot has started development of a tool to accomplish the shearing operation. Tests run at LMEC using their prototype indicated that the approach is practical; however, the design of the prototype had several serious disadvantages. The prototype was returned to Microdot with LMEC comments and recommendations.

The ability to predict apparent strain characteristics by calibrating the gage in an unmounted condition will also be investigated.

3. Test ST27

Significant improvement had been noted during the ST18 test series when the pressure trip spot-welding technique was used in place of the Microdot

Rollelectrode. This weld pattern was the same as that used in the ST13 and ST14 tests (comparison of chisel and mill-saw removal techniques). As a result of these previous tests, it was concluded that a special weld pattern should be developed for each separation technique. The weld pattern used on the first installation in the ST27 testing is adjacent to the gage flange for chisel separation; and that for the mill-saw is spaced approximately midway between the edge of the intermediate shim and the gage flange to provide clearance for the cutting blade.

The welding techniques for the first installation were refined by using a fixture which held the calibration and provided clamping pressure on the gage assembly. The spot-welding head was attached to the work bench and operated by means of a foot lever. The gage fixture was mounted on a two-axis micrometer cross-feed table for positioning under the welder. This allowed close control of weld line position and weld spacing.

The second application of the gage assembly attempted to simulate that of a field-type installation. A hand-held pressure trip probe was used without any fixtures and no microscope or other visual aid. The weld spacing was determined strictly by the skill of the technician. A single weld line was made on the gage flange between the two existing rows of welds attaching the gage to the intermediate shim. A pressure of 10 lb and a power setting of 35 watt-sec was found to provide consistent welds through the two thicknesses (gage flange and intermediate shim to the calibration bar).

a. Test Procedure and Description

Twelve Microdot SG420 gages were spot-welded to six 1/4 by 1 by 12-in. calibration bars. The surface of the calibration bar was polished to a No. 8 microfinish.

The gage assemblies were then stabilized at 1100°F for 16 hr. Two cycles of apparent strain from room temperature to 900°F were recorded. Three load tests were recorded at each of the following temperatures: room temperature, 500°F, and 900°F. Maximum load was approximately 700 micro-strain and was applied using the constant-moment deflection fixture. Following the load tests, one apparent strain cycle was recorded. The gages were then removed using a single-edge razor blade as a chisel.

The gages were reinstalled on the same bar using a pressure-trip hand probe on the spot-welder. A single row of welds was placed on each side of the strain tube on the strain flange (welding through the strain flange and intermediate shim to the calibration bar). Weld spacing was approximately 25/in.

Two apparent strain cycles were recorded from room temperature to 900°F and returned to room temperature. Three load cycles were recorded at each temperature as was done on the first installation. This was followed by one apparent strain cycle.

b. Test Results

Preliminary results from this test are as follows:

	<u>Room Temperature</u>	<u>500°F</u>	<u>900°F</u>
Gage Factor			
Average Change (%)	4.2	2.5	1.9
95 to 90% Tolerance Range (%)	±6.2	±5.2	±4.5
Apparent Strain			
Average Change ($\mu\epsilon$)	-	13	9
95 to 90% Tolerance Range ($\mu\epsilon$)	-	±84	±89

c. Conclusions

The results from this test show a definite improvement in all gage characteristics from previous tests. The greatest improvement is in the apparent strain. The small average change from calibration to final installation is due primarily to the small change in weld geometry, and to close control of the weld operation. The contribution of the polished surface cannot be readily determined from this test, but it is felt that it does improve the consistency of the welds.

4. Test ST28

This test was performed to evaluate a special weld pattern to allow mill-saw separation of the SG420 strain gage after calibration. The weld pattern on the initial installation differed from that of test ST27 in that the weld lines were placed approximately halfway between the edge of the gage flange and the

edge of the intermediate shim. This was done to provide adequate clearance for the gage lead junction and strain relief of the leadwire.

The data from this test are presently being evaluated. Preliminary results indicate a much larger average shift in both gage factor and apparent strain with a larger scatter (tolerance range) in both characteristics. This is attributed to the greater weld line spacing and is not believed inherent in the mill-saw techniques. A revised gage lead junction and strain relief is being designed to allow duplication of the weld pattern used for test ST27 and still allow use of mill-saw separation. It is believed that the combination of the ST27 weld pattern and mill-saw separation will provide the best possible calibration technique for the SG420 strain gage.

5. Test ST29

This test is being performed to evaluate the SG425 strain gage on an individual calibration basis. The weld pattern is the same as that used on the SG420 gage installation for test ST27 for both initial and final installation. Mill-saw separation is used to remove the gage after calibration.

The test is in progress and no results are available for this report.

C. CONSTANT MOMENT BENDING CALIBRATION FIXTURE

The constant moment bending calibration fixture has been used successfully in the LMEC strain gage laboratory for the past several years during the evaluation of a variety of high-temperature electric resistance strain gages. Since the primary purpose of evaluating those gages was to make comparative measurements of changes in gage characteristics, as a function of time at temperature, extreme precision of measurement in terms of absolute accuracy was unnecessary.

Microdot SG420 and/or SG425 strain gages will be used to determine stresses at critical locations on the Alco/BLH steam generator and Alco IHX which will be tested at the SCTI. To obtain the maximum possible accuracy from these gages, each gage will be precalibrated, prior to installation. The construction of the Microdot gage is such that the strain sensing filament is approximately 0.027 in. away from the surface to which the gage is attached, the position varying from gage to gage. Under axial loading, this introduces no complications in the calibration of the gage.

Under flexural (bending) loadings, a correction must be applied to the strain gage calibration data to account for the filament "standoff." To further complicate matters, the correction varies from gage to gage, introducing an uncertainty in the magnitude of the correction to be applied. Because of these complications, it was tentatively decided that the constant moment bending fixture should not be used for gage precalibration purposes. Instead, an axial loading fixture was designed, fabricated, and checked out for use. By using special flexures, it was possible to eliminate any spurious bending resulting from machine misalignment friction, or any other external sources of bending. By using an axial calibration bar machined and ground to very close tolerances with respect to concentricity and uniformity, bending within the bar caused by specimen bow, as well as variations in strain within the gage length, was eliminated. Careful checks with room-temperature electric resistance strain gages, mounted every 90 degrees around the circumference at various locations along the gage length, verified that the bending was truly negligible.

Although considered satisfactory at the time, the Gaertner microscope is no longer considered adequate to meet the more stringent accuracy requirements sought for in an instrument for calibration of the axial load calibration bar. In addition, it was decided that each Microdot gage to be installed on the steam generator and IHX should be X-rayed for quality assurance reasons. This automatically provided a means of determining the location of the strain sensing filament within each gage to a much higher degree of accuracy than possible by statistical sampling of a small percentage of the gages from each gage lot. With the X-ray information, it will be possible to eliminate the above-mentioned uncertainty of the magnitude of the standoff correction to be applied to bending calibration data.

The bending calibration bars presently used for routine evaluation of gages in the LMEC strain gage laboratory are 1/4 in. thick by 1 in. wide by 12 in. long. It was found that a bar at least 3/8 in. thick by 1-1/4 in. wide by 12 in. long could be accommodated in the existing constant moment fixture without any modification of the fixture. By using a thicker and wider calibration bar, the reinforcing effect and the strain filament standoff corrections are substantially reduced, resulting in still greater accuracy with a bending calibration.

It should also be noted that the strains induced in the bending calibration bar are independent of the materials properties of the bar or creep in the bar occurring during tests. This is not so with the axial calibration bar.

Because of the above considerations, it was decided that further investigations with the axial loading fixture would be held in abeyance, at least until a more accurate high-temperature extensimeter became available; and that the effort should be redirected toward the use of the 3/8 by 1-1/4 by 12-in. bending calibration bar. The following section of this report contains the error analysis applicable to that bar.

1. Error Analysis

A "worst-case" error analysis was performed for a single gage assembly configuration, located at the center of a 3/8-in.-thick by 1-1/4-in.-wide by 12-in.-long Type 316 stainless-steel calibration bar. In addition, analysis of a second configuration, consisting of four gages symmetrically arranged about the bar centerlines (dual-tandem arrangement), was completed.

The analyses assume a worst-case situation in which all errors are of the same sign (additive), whereas, in reality, some are of opposite sign and cancel each other. The analyses take into account all errors of significance, including those due to specimen and fixture tolerances, stiffening effect of gage assembly, shift in neutral axis of calibration bar due to presence of gage, and strain sensing filament standoff.

Mathematically, it can be shown that if

$$y = f(u, v, w, x, \dots) \quad ,$$

where u, v, w, x , etc., are the independent variables, that

$$dy = \frac{\partial y}{\partial u} du + \frac{\partial y}{\partial v} dv + \frac{\partial y}{\partial w} dw + \frac{\partial y}{\partial x} dx + \dots \quad ,$$

where du, dv, dw, dx , etc., are the differentials (errors) associated with each variable.

For the error analysis presented herein, the corresponding functional relationship is

$$d\epsilon = \frac{\partial \epsilon}{\partial \Delta} d\Delta + \frac{\partial \epsilon}{\partial d} dd + \frac{\partial \epsilon}{\partial \bar{y}} d\bar{y} + \frac{\partial \epsilon}{\partial w} dw + \frac{\partial \epsilon}{\partial a} da \quad ,$$

where

ϵ = strain on surface of calibration bar at gage location (microstrain)

a = distance between point of support and point of loading on calibration bar (in.)

d = bar thickness (in.)

Δ = deflection at center of calibration bar (in.)

$\bar{y}$ = shift of neutral axis due to presence of strain gage assembly (in.)

w = correction factor for gage standoff

$d\Delta$, dd , $d\bar{y}$, dw , and da = magnitudes of the assigned errors in Δ , d , y , w , and a , respectively.

It should be noted that the value of the errors assigned to the variables were determined on the following basis:

- 1) Error $d\Delta$: that deflection dial gage, whose least count (value of smallest division) is 0.00001 in., can be read to an accuracy of ± 0.00005 in.
- 2) Error dd : that the average thickness of the bare calibration bar can be determined to ± 0.00001 in.
- 3) Error $d\bar{y}$: that the shift in the neutral axis can be determined with confidence, from section properties calculations, assuming geometric tolerances for gage strain tube, gage flange, intermediate shim, and calibration bar.
- 4) Error dw : that the location of the strain sensing filament can be established on the basis of the 95% confidence limits for the 22 Micro-dot gages, whose measurements are summarized in Table 16. These measurements were made from photographic prints of the X-ray

negatives of the gages. Actually, the error, dw, as explained earlier, will be less than that predicted by the 95% confidence limits, since it is now proposed to determine, by actual measurement, the location of the filament for each gage.

- 5) Error da: that misalignment between opposing knife-edge supports, and shift in point of contact of knife edges (due to rotation of calibration bar) can be measured to 0.001 in.

TABLE 16
STATISTICAL DATA, STRAIN SENSING FILAMENT LOCATIONS

Gage No.	Upper Midpoint	Lower Midpoint	Upper End	Lower End	Case Diameter	X	X ²
19	16.0	12.0	20.5	12.5	30.0	19.3166	373.1310
4	21.5	18.5	20.0	18.0	30.5	24.2951	590.2519
5	22.0	16.0	22.0	12.5	27.5	25.0455	627.2770
2	19.5	15.5	18.5	17.0	27.0	24.8056	615.3177
16	20.0	14.5	27.5	9.0	29.5	22.8644	522.7808
3	16.0	12.0	24.0	16.0	28.5	22.6667	513.7793
8	14.0	10.0	18.0	9.5	27.0	18.1204	328.3489
15	20.0	11.5	24.0	9.0	29.5	20.7712	431.4427
9	13.0	9.5	16.0	9.0	27.0	16.7130	279.3244
6	22.0	18.0	21.0	11.0	27.0	25.3333	641.7776
12	22.5	13.5	25.0	11.5	31.5	21.8651	478.0826
14	16.5	11.0	13.0	12.0	28.0	17.8125	317.2852
23	12.0	8.5	13.5	8.0	27.0	14.7778	218.3834
22	17.0	15.5	14.5	13.0	28.5	20.0000	400.0000
1	15.5	10.0	19.5	11.0	28.0	19.0000	361.0000
10	22.5	16.0	22.0	14.5	28.0	25.4464	647.5193
11	24.5	13.5	19.0	15.0	29.0	23.5862	556.3088
20	23.5	21.5	20.5	19.0	34.5	23.2681	541.4045
18	12.5	11.0	18.5	9.5	29.0	16.8706	284.6171
21	20.0	13.0	19.5	10.0	28.5	20.8333	434.0264
17	18.5	9.5	16.0	8.0	28.0	17.6429	311.2719
13	17.0	13.5	15.0	12.5	31.5	17.4921	305.9736
Σ						458.5262	9,779.3036

Note:

$$\bar{X} = \frac{458.5262}{22} = 20.8421 \text{ mils (not including 6-mil-thick shim)}$$

$$\bar{X}^2 = \frac{9,779.3036}{22} = 444.5138$$

Between the points of application of the loads P, which are generated by the deflection, Δ , the bending moment, M, is constant and equal to Pa. From elementary bending theory, it can be shown that the strain on the surface of the calibration bar, within the constant moment section, is

$$\epsilon = \frac{Mc}{EI_0} = \frac{PaC}{EI_0} \quad , \quad \dots(1)$$

where

c = distance from neutral axis to surface of bar (= 0.5 bar thickness)

EI_0 = bending modulus of the bare bar (lb/in.²).

If $\bar{y}$ is the shift of the neutral axis due to the presence of the strain gage assembly and W the correction factor to account for strain filament standoff, then Equation 1 becomes

$$\epsilon = P \left[\frac{a(0.5d - \bar{y})}{EI_2 W} \right] \quad , \quad \dots(2)$$

where EI_2 is the bending modulus, the strain gage assembly - bar combination.

The magnitude of the load P is unknown; however, the magnitude of Δ is known from the dial gage reading. Although P is unknown, it may be determined in terms of Δ by application of the area-moment solution for a beam of variable moment of inertia. It should be noted that the strain gage assembly reinforces the calibration bar and effectively increases the moment of inertia over that portion of the bar to which the assembly is attached. This results in a bar with variable moment of inertia. Applying the area moment solution, it can be shown that

$$P = \frac{3EI_0}{[a^3 + 5.918031 a^2 + 5.879481 a]} \Delta \quad \dots(3)$$

Substituting the expression for P in Equation 3 in Equation 2, the following expression for E is obtained for the single-gage configuration:

$$\epsilon = \frac{3\Delta (0.5 d - \bar{y})}{W (a^2 + 5.918031 a + 5.879581)} \quad \dots (4)$$

The term W in Equation 4, as mentioned earlier, is a standoff correction factor to account for the fact that the filament is located approximately 0.027 in. away from the surface of the calibration bar. It was determined as follows:

$$\bar{x} = 0.0268421 \text{ in.}$$

$$x_u = 0.0282860 \text{ in.}$$

$$x_l = 0.0253982 \text{ in.}$$

$$D_1 = \bar{x} + d/2 = 0.0268421 + 0.18586 = 0.212702 \text{ in.}$$

$$D_2 = x_u + d/2 = 0.0282860 + 0.18586 = 0.214146 \text{ in.}$$

$$D_3 = x_l + d/2 = 0.0253982 + 0.18586 = 0.211258 \text{ in.}$$

$$\text{Ratio based on mean value} = \frac{d/2}{D_1} = \frac{0.18586}{0.212702} = 0.873805$$

$$\text{Ratio based on upper C.L.} = \frac{d/2}{D_2} = \frac{0.18586}{0.214146} = 0.867913$$

$$\text{Ratio based on lower C.L.} = \frac{d/2}{D_3} = \frac{0.18586}{0.211258} = 0.879777$$

$$1/2 \text{ confidence interval} = \frac{0.879777 - 0.867913}{2} = 0.0059$$

Therefore $W = 0.873805 \pm 0.0059$.

For the dual-tandem configuration, the strain equation corresponding to Equation 4 is

$$\epsilon = \frac{3\Delta a (0.5 d - \bar{y})(0.95018)}{W(a^3 + 5.6822 a^2 + 5.7583 a - 0.005492)} \quad \dots (5)$$

Table 17 summarizes the partial derivatives of ϵ , with respect to Δ , d , $\bar{y}$, W , and a , for both configurations. These derivatives were obtained by taking the partial derivatives of the strain, ϵ , with respect to each variable in Equations 4 and 5. Table 18 summarizes the numerical values of the assigned error differentials $d\Delta$, dd , $d\bar{y}$, dW , and da for both configurations.

TABLE 17
SUMMARY OF PARTIAL DERIVATIVES

Partial Derivative	Single Gage Configuration ($\times 10^{-3}$)	Dual-Tandem Gage Configuration ($\times 10^{-3}$)
$\frac{\partial \epsilon}{\partial \Delta}$	16.005	15.8684
$\frac{\partial \epsilon}{\partial d}$	3.2293	2.15217
$\frac{\partial \epsilon}{\partial \bar{y}}$	6.4586	-4.30434
$\frac{\partial \epsilon}{\partial w}$	-1.37376	-0.908000
$\frac{\partial \epsilon}{\partial a}$	0.399217	-0.265545

TABLE 18
SUMMARY OF ERROR DIFFERENTIALS

Error Differential	Single Gage Configuration (in.)	Dual-Tandem Gage Configuration (in.)
d Δ	± 0.00005	$+0.00005$
dd	± 0.0001	± 0.00010
d $\bar{y}$	± 0.000151	± 0.00028655
dw	± 0.005972	± 0.005972
da	± 0.001	± 0.001

The total error, dE, was obtained by substituting the values in Tables 17 and 18 into the total error equation,

$$d\epsilon = \frac{\partial \epsilon}{\partial \Delta} d\Delta + \frac{\partial \epsilon}{\partial d} dd + \frac{\partial \epsilon}{\partial \bar{y}} d\bar{y} + \frac{\partial \epsilon}{\partial a} da \quad \dots (6)$$

From Equation 6, for the single-gage configuration, the total error was found to be $\pm 7.4016 \times 10^{-6}$, for a deflection $\Delta = 0.050$ in. and strain, computed from Equation 4, of 800.3×10^{-6} . The percent total error is therefore

$$\frac{d\epsilon}{\epsilon} \times 100 \text{ or } \pm 0.925\%$$

For the dual-tandem configuration, the total error $d\epsilon$, was found to be $\pm 7.9302 \times 10^{-6}$ for a $\Delta = 0.050$ in. and a strain, ϵ , of 793.42×10^{-6} . The percent total error, $(d\epsilon \times 100)/\epsilon$, is $\pm 0.9995\%$.

It should be noted that the deflection $\Delta = 0.050$ in. is the maximum value of deflection believed possible in a 3/8-in.-thick bar, without introducing yield in the calibration bar during elevated temperature calibration. This deflection produces approximately the same strain in a 3/8-in.-thick bar that the 0.075 deflection produces during strain gage calibration with a 1/4-in.-thick bar.

2. Comparison of Accuracy of Bending Calibration Fixture with Axial Loading Fixture

Data obtained from the axial loading calibration fixture test included a determination of accuracy of the fixture in terms of confidence limits, systematic error, and standard error. The upper and lower 95% confidence limits were determined for all three axial load calibration bars from room temperature to 900°F. From the mean values and upper and lower confidence limits, the deviation from the mean values (expressed in percent) were found to vary from 1.182 to 2.592%. The overall error for all three bars varied between ± 3.6 and $\pm 4.0\%$.

It is evident that the constant moment bending fixture worst-case errors of $\pm 0.925\%$ for the single gage, and $\pm 0.9995\%$ for the dual-tandem configuration are substantially less than those calculated for the axial load fixture. It is therefore recommended that calibration of the gages for the Alco/BLH steam generator and Alco IHX be done with the bending calibration fixture, utilizing a 3/8-in.-thick calibration bar.

D. STRAIN GAGE SYSTEM ERROR ANALYSIS

The basic problem is to determine the error associated with a measurement. In this case, the measurement is strain. Input to the measurement system is actual strain, ϵ , on the test article, and the output is the corrected measured strain, ϵ_T . This results in an error $\tau = \epsilon - \epsilon_T$. Since ϵ is an unknown quantity, τ will also be unknown. It is possible, however, to derive an estimator which will provide an estimate of τ which represents the "total error" for a given operating condition of the system.

In the measurement of strain, the total error is comprised of a number of individual errors that will be called "component errors." Each component error will be dependent on from one to three of the independent variables associated with the strain gage system. These independent variables are temperature, time, and strain level.

Development of the estimator τ for the total error can be divided into the following tasks:

- 1) Definition of the component errors.
- 2) Combining the component error functions into a model which will serve as a τ estimator.
- 3) Collection of data for component error evaluation.

1. Definition of the Component Errors

A detailed analysis will be made to identify the component parts of the measurement system. In general, the measurement system consists of the strain gage, the signal conditioning equipment, interconnecting wires, the multiplexer, the analog-to-digital converter, the computer, and the output device. Figure 69 is a block diagram of the system. Each of these categories has one or more component errors. For example, a strain gage has multi-component errors: (1) the measurement of the apparent strain owing to the difference in the thermal expansion between the gage and the test article; (2) the measurement of change in gage resistivity owing to temperature; (3) the gage drift characteristics with respect to time; (4) the removal from the calibration bar; and (5) the reinstallation of the gage on the test article, etc.

For each category of the measurement system, it will be necessary to define the component error as to cause, independent variables involved, and significance. These definitions will be documented in enough detail to justify the decision regardless if the error is negligible or significant. This documentation will also provide information as to the type of data needed to establish component errors and will be used in test planning or in establishing the method of estimation if testing is not possible. Actual determination of the estimator, τ , of the total system error will be based on the decisions concerning component errors; so the importance of these decisions cannot be overemphasized.

Figure 69. Strain Measuring System

2. Combining Component Error Functions to Estimate τ

A computer program (SYSMSG) has been developed at LMEC to determine the value of τ ; a flow chart is presented in Figure 70. The input to this program includes a series of statistical means ($\bar{X}$) with their associated confidence limits (U and L) expressed in terms of microstrain for each component error being considered. These values of $\bar{X}$, U, and L will be obtained from the calibration tests, operating tests, and estimation methods established for each component in the measurement system. The values will be calculated in standard statistical equations:

$$\bar{X} = \frac{1}{n} \sum_{i=1}^n \chi_i ; U = \bar{X} + t \frac{s}{\sqrt{n}} ; L = \bar{X} - t \frac{s}{\sqrt{n}} ;$$

where

n = number of data points

χ = data value

t = Student's distribution factor based on n - 1

s = standard deviation.

Along with these data, an input of the operating conditions (temperature, time, strain level) being evaluated will be required. The most probable error (and its computational accuracy) will be the primary printout.

The main portion of the program inputs data, outputs the results, and calculates the most probable error τ :

$$\tau = \sqrt{\sum_{i=1}^n (e_i^2)} ,$$

$$e = \frac{1}{2} (U - L) ,$$

where e = component error.

Figure 70. Basic Flow Chart— SYMSG

A library subroutine called CODIMA (controlled deviation interpolation method) is a curve-fitting routine which will be used to determine the component errors. This routine was chosen because of its ability to fit corners or sharp turns without the large deviation found in other methods. From LMEC's present experience with the measurement system components, this approach was appropriate. The method will interpolate in the following manner:

- 1) The first derivation is continuous except at the ends of straight segments defined by three points on a straight line.
- 2) No large deviation will be found when slope changes are large.
- 3) Ability to change value and slope rapidly.
- 4) Ability to fit straight line portions of the curve and fit smooth arcs through the smooth portions of the curve.

In this method, data from "previous points" (of the curve) are used to fit a polynomial through the defined interval and data from "subsequent points" (of the curve) are used to fit a second polynomial. These two polynomials are compared for compatibility. If they differ, a weighted average of the polynomials is taken in such a way that the polynomial that deviates less from the straight line connecting the points defining the interval is given more weight. Parabolas rather than high-degree polynomials are used in this routine.

A list of specific operating conditions, strain gage characteristics, and data acquisition methods is being prepared for a final checkout of this analysis. This will be done prior to completion of the definition of component errors, since the computer program is sufficiently flexible that additional component errors can be included as needed.

In investigating the entire strain gage program with regard to reduction of calibration data and of test data, a method which combines the system error analysis as an additional step is being developed. The error analysis program described above forms one part of the overall procedure to be used for strain gage data reduction. The block diagram in Figure 71 depicts the typical functions which will be accomplished with data resulting from strain gage testing. Functions include calibration, test data recording, data reduction, and data analysis. As part of the finalized data analysis, results from the error analysis will form an influence on the determination of the quality of the conclusions.

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Figure 71. SCTI Steam Generator/IHX Strain Gage Data Reduction Procedure

The program makes use of observed variations in the calibration data to estimate the uncertainty (error) introduced during the calibration procedure. By applying similar analyses to subsequent calibration data taken after re-installation of strain gages, an estimate of the error introduced during reinstallation of gages on the test article can be deduced. The program makes use of other data such as that depicting gage drift characteristics, perturbations of signal-conditioning programs to determine computer software errors, and other known effects. This program will use a least-square curve-fitting technique which develops polynomials to represent the various error functions. The printed output from the computer will include the total error for any operating condition (temperature, strain level, time) as a root-mean-squared sum and the individual error values for standard deviations and upper and lower confidence limits.

3. Collection of Data for Component Error Evaluation

Data used to evaluate the component errors will be test data or estimated data. In either case, care must be taken that it actually represents the error function being defined.

The written documentation that defines the component errors will be used to determine test and estimation methods to collect these data. This means that methods already established will be reviewed and modified as required, any new procedures will also be reviewed before they are accepted, and techniques will be developed for areas not yet covered. A plan to ensure that all changes to the system are incorporated will be utilized.

ID SURFACE 7693-4723

7693-4722

Figure 72. LCTL Pipe Section at Sodium Leak

←
Figure 73. LCTL Pipe Wall Showing Leak Path
(Mag: 100X - Reduced to 66%)

VIII. CHEMICAL AND METALLURGICAL LABORATORY
(LMFBR Program Plan 3-3.1, 5-5.1 through 5-5.7)
R. K. Wagner

A. INTRODUCTION

The objective of this program is to provide chemical and metallurgical laboratory support for LMEC activities, and LMFBR activities as requested.

B. FAILURE ANALYSES - LCTL PIPING FAILURE

A sodium leak was found in a section of LCTL piping where evidence of intergranular corrosion had been previously detected.* This failure - in a 3-in.-diameter Type 304 stainless steel pipe section - was detected during a visual examination which followed a 160-hr "hot circulation" test. As shown in Figure 72, the leak is minor in appearance (estimated sodium loss, less than 1 cc) and occurred near a pipe hanger attachment weld. Metallurgical examination indicated that intergranular corrosion resulted in wall penetration at two locations. The smaller of the leak paths is shown in Figure 73. Composition of the pipe material was verified as Type 304 stainless steel with a carbon level of 0.06 wt %. Laboratory results indicate that this leak was produced by intergranular attack of sensitized stainless steel. The corrosive attack originated on the outside surface of the pipe, by corrodants associated with the thermal insulation but not specifically identified.

After continued operation of LCTL, a second sodium leak of minor proportions was detected in a sodium service line to the plugging meter. This leak, a continuous circumferential crack (Figure 74), occurred in a stainless steel elbow adjacent to a weld in a 1-in.-diameter pipe. The "cleansing action" of the leaking sodium on the surface oxide surrounding the crack is apparent. The discontinuous character of the crack that penetrated the elbow wall is shown in Figure 75. A photomicrograph of the crack area (Figure 76) clearly shows the intergranular nature of the penetration, which is characteristic of a type of corrosion common for sensitized austenitic stainless steels. Examinations at higher magnifications showed a heavy network of grain boundary precipitation typical of well-sensitized stainless steel.

Subsequent examination of adjacent pipe and finned-tube sections revealed additional evidence of intergranular corrosion. Figure 77 represents the structure and intergranular cracking that penetrated a pipe wall located approximately 1 in.

*LMEC Semiannual Technical Progress Report, January-June 1969, LMEC-69-30

from the circumferential crack (Figure 74). Dye penetrant examinations of beveled joints prepared for welding were instrumental in revealing surface indications in still other areas. After removal from the system, these areas also showed evidence of intergranular corrosion. As shown in the photomicrograph (Figure 78), the attack was initiated on the external surfaces; there was no evidence of a partial penetration crack network that originated from the inside surfaces. During initial examination of the failed elbow, it was noted that the thermal insulation removed from the failure area was wet. Based on the characteristics of the failed area and the environment, it is apparent that this and the previously described failure are nearly identical.

As previously noted, failure experiences such as these point out the need for adopting certain precautionary measures when dealing with sensitized austenitic stainless steels. Without qualification, they must be protected from moisture or condensates which can form corrosive media during downtime or periods of low-temperature operation.

7693-25122

Figure 74. Sodium Leak Area in Elbow Section of Plugging Line
(crack is continuous ~180 deg around circumference)

7693-25123

Figure 75. Inside Diameter Section in Leak Area
(crack extends through wall thickness but is
discontinuous on ID)

Figure 76. Photomicrograph of Intergranular Penetration in Heat-Affected Zone of Circumferential Weld (Leak Area) (Mag ~ 50X)

Figure 77. Intergranular Penetration in Finned Tube Section (Mag ~ 100X)

7693-25126

Figure 78. Typical Intergranular Penteration
of OD of Elbow (Mag $\sim 100X$)

IX. LCTL/SCTL OPERATION, MAINTENANCE, AND MODIFICATION

(LMFBR Program Plan 3-5, 3-6, 4-1)
L. B. Copeland

A. INTRODUCTION

It is the responsibility of this project to operate the Large Components Test Loop (LCTL) for LMFBR and FFTF component and instrument testing; to modify the LCTL to permit operation at LMFBR temperature and pressure conditions; and to maintain the Control Element Test Stand (CETS). Planned tests to be performed in the LCTL during FY 1970 and 1971 are the FFTF Flow Duct/Tube Sheet Sodium Erosion-Flow Leakage Test (T-1B), a second such FFTF Test designated T-1D, and, tentatively, the FFTF Fin Tubing Test (T-2).

B. LCTL OPERATION AND MAINTENANCE

A reexamination and an evaluation were made of the 3-in. loop in which defects had been found which were primarily attributed to intergranular corrosion of the stainless steel pipe (sensitized by previous service; see Section VIII of this report for additional information). Visual and liquid-penetrant inspections were repeated at the same locations which had been examined prior to the hot circulation test previously reported. A minute sodium leak (less than 10 gm) was discovered under the thermal insulation near a pipe-hanger weld on a 21-in. - long section of the piping. The section was removed for metallurgical examination and a new piece was installed. The metallurgical examination disclosed extensive intergranular corrosion in the leak area, originating on the outer surface and progressing through the wall; in areas away from the leak, penetration was 0.001-in. or less. Cracks, visible on the inner surface of the pipe where the leak occurred, were not indicated by liquid-penetrant inspection of the exterior surface. However, both single-wall and double-wall radiographs clearly showed the cracks.

Liquid -penetrant test recordings were made (using pressure-sensitive tape and a high-resolution liquid penetrant) and compared to recordings made in the same locations on the loop prior to the hot circulation test. Comparison of the two sets disclosed no change on the exterior surfaces. Additional penetrant inspections were made adjacent to the welds of another pipe hanger in the loop. While the test indicated a possible crack, subsequent radiographs disclosed no defect.

While it was recognized that leaks similar to that experienced could develop, it was judged best to continue with the planned use of the loop for the BNW/FFTF T-1 tests. As a contingency measure, a design was made and materials procured for a modification that could make the T-1 test loop independent of the 3-in. loop. The modification is planned only if warranted by extensive repair requirements or a high rate of failures. A plan of action has been prepared for further examination of the loop to be made at the conclusion of the T-1 tests. Future utility will be judged from the information available after that examination.

Repair of the rotary EM pump to have been used for FFTF Test T-1B was unsuccessful. Failure to meet expected performance had been attributed to degradation of a braze alloy used on the pump duct to make the electrical path for eddy current flow. (The duct consists of a 10-turn helical coil of tubing with the braze alloy filler between adjacent coils.) The braze fillet was removed from between the coils on the outside of the duct and a new fillet was put in by torch brazing. Pump performance tests were repeated and initial data indicated a significant improvement; however, in only a few hours of operation, performance had fallen to essentially the same values observed prior to rework.

Because duct repair was unsuccessful and lead time for procuring a new duct was quite long, attempts to locate a substitute pump were repeated. As a result, two helical induction EM pumps were obtained which, in combination, proved to meet performance requirements (Figure 79). While in initial tests, pump performance did not meet expectations. This was determined to be caused by gas in the system, which was removed by valving to use the core tank as a flow-through expansion tank. Tests were made with both single and dual pump operation in both series and parallel flow configurations. Performance proved to be superior in the parallel flow configuration. A differential pressure of 158 psig was developed across the pump performance test section at a flowrate of 47 gpm. At this point, the power to each pump was 145 amp.

Fabrication of the new duct for the rotary EM pump was continued until the helical induction pumps were performance tested. Attempts to produce a satisfactory braze fillet were unsuccessful and an alternate method was being investigated when the effort was stopped. This was a TIG welding method which appears feasible, although costly.

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Figure 79. Helical Induction Pumps Installation at LCTL

The freeze-seals of the mechanical pump in the 3-in. loop were reworked and the pump was tried again, coincident with the final test of the rotary EM pump. During the test, the case freeze-seal was lost and the pump bucket was partially flooded. A team was appointed to investigate the several freeze-seal malfunctions. Various tests were made on the pump, cooling system, and instrumentation. Comparative tests were made on the nearly identical sodium transfer pump (and it was then discovered that the transfer pump, though operable, was also flooded with sodium). While the investigation was not decisive, it has been tentatively concluded that the freeze coolant passages, in which water is guided by vanes, suffer from deteriorated vanes, thus permitting coolant bypass and loss of freeze seal. Since pump operation is not necessary for the T-1 tests, further action has been deferred until test completion and examination of the 3-in. loop.

While attempting to determine the plugging temperature of the reserve sodium in the system (i. e., the sodium in the supply and drain tanks), a small sodium leak was observed in the plugging loop area. Examination disclosed a circumferential crack in a pipe elbow immediately adjacent to the weld joining the elbow to the finned tube of the plugging loop cooler. Metallurgical examination disclosed that the failure was caused by extensive intergranular corrosion of the elbow. The finned tube and adjacent sections of pipe were similarly corroded.* The plugging-temperature measurement and cold-trapping of the reserve sodium were resumed after replacing the defective materials.

On December 30, 1969, plugging-temperature measurements were made of the sodium in the T-1B and 3-in. loops (and, consequently, of the core tank since all were valved in a series-flow configuration). Although the system had initially been filled with sodium having a plugging temperature of 261°F and the system had not been opened, the plugging temperature was found to be 555°F. Chemical analyses of sodium taken only 8 and 22 days previously had shown respective oxygen concentrations of 13 and 19 ppm. Further investigation and resolution of necessary corrective measures were deferred to January 2, 1970.†

*Prior to installation in the LCTL, all of the defective plugging-loop materials had been used in another sodium system. During this previous service, the materials had been operated at 1000 to 1200°F for long periods of time; hence, they had been sensitized prior to use in the LCTL. Since service in the LCTL is limited to 700°F operation, other portions of the plugging loop are not similarly affected.

†Subsequent plugging measurements were 560 and 555°F. Chemical analysis of another sodium sample showed an oxygen concentration of 35 ppm. The system configuration was changed to closed-loop operation, using the supply tank as the expansion tank, and the loop was flushed with supply tank sodium having a plugging temperature of 265°F. Cold trapping was started of all the sodium not in the 3-in. and T-1B test loops. Test operation was interrupted only when plugging measurements were being made.

On several occasions, certain of the freeze-stem sodium valves have not operated upon demand. This is a recurring problem which is not uncommon with valves of this type in sodium systems. The major cause is attributed to the accumulation of sodium oxide on the stem from repetitive exposure to air of the portion of the stem covered with a film of sodium. As the oxide accumulation increases, the force required to operate the valve can increase until the capacity of the valve operator is exceeded. Corrective measures normally consist of heating the freeze-seal zone with heater cable until the valve is operable, then cleaning the stem above the packing gland with the valve in the open position. Occasionally, a closed valve must be jacked open.

As an attempt to alleviate the problems of such valve malfunctions, a modification was designed and will be tried after design-review approval. The proposed modification, which can be made in the field without removing the valve, employs a flexible cover (boot) surrounding the stem to permit purging and to exclude air, hence, reduce the rate of oxide accumulation.

C. FFTF FLOW DUCT/TUBE SHEET SODIUM EROSION-FLOW TEST (T-1B)

As previously noted, two helical induction pumps secured from ORNL were installed in the test loop to provide the sodium pressure and flow requirements for FFTF Test T-1B. The test plan and test procedure were revised to reflect the use of these pumps and the incorporation of a change requested by BNW/FFTF to operate at 750 rather than 800°F (made subsequent to an agreement to operate at 800 instead of 900°F).

While preparing the T-1B test assembly for installation, numerous metal chips and foreign particles were found inside both headers. The test assembly was cleaned and examined according to a procedure agreed upon by BNW and LMEC. After reviewing the results of the examination, it was concluded that the test assembly had been adequately cleaned and should be installed for testing.

Installation was completed (see Figure 80) and the test assembly and loop were filled with sodium on October 22, 1969. Following initial checkout of the test assembly, steady-state operation was begun on October 26, 1969, with a design ΔP across the test section of 140 psi and a bulk sodium temperature of 750°F.

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Figure 80. FFTF Flow Duct/Tube Sheet Sodium Erosion-Flow Test (T-1B) Installation at LCTL

During the first eight days of testing, flowrates through the test assembly followed a decreasing trend. Module T1-5 (largest clearance) had an indicated flowrate of 20.0 gpm during the checkout phase and the sum of all module flows was 34.4 gpm. One day later, module T1-5 flowrate had dropped to 14.8 gpm and the module sum was 29.3 gpm. This was at the start of steady-state operation. By November 2, 1969, module T1-5 flow had decreased to 13.6 gpm and the module sum had decreased to 27.8 gpm.

On suspicion of solid particles clogging the test modules, a plan for back-flushing the assembly was coordinated with BNW. The back-flush operation was performed on November 4, 1969, after which flowrates showed a slight recovery. However, flowrate through module T1-5 (15.4 gpm) was still significantly less than its value at the start of the test. The module sum was 29.7 gpm.

Another attempt was made to bring the flowrate through module T1-5 back to its expected value, this time by vibrating. On November 14, 1969, per agreement with BNW personnel, the T1-5 module was vibrated for 8 intervals of 10 sec each. After each interval, except the last, an increase in flow through the T1-5 module was observed. At the end of the vibrating operation, module T1-5 flowrate was 18.0 gpm and the module sum was 32.3 gpm.

Continuing on a downward trend after vibrating, module T1-5 flowrate decreased gradually until November 20, 1969, when the flow leveled off at 17.5 gpm. Similarly, the module sum decreased to 31.8 gpm during the same period. The flows remained constant until November 26, 1969, when an upward trend in T1-5 flow was detected. By December 15, 1969, module T1-5 flow had increased steadily to 17.9 gpm and the module sum had increased to 32.2 gpm. For a period of one week after December 15, 1969, steadily decreasing values of flow were observed for all test modules. These indicated flow changes were subsequently attributed to an instrument error; after instrument correction on December 22, 1969, the flow through module T1-5 was indicated at 18.3 gpm while the module sum was 33.0 gpm. Then, continuing on an upward trend, the module T1-5 flowrate reached a value of 18.5 gpm and the module sum reached 33.6 gpm on December 30, 1969. On that date flow was halted in order to make plugging runs with the sodium being used in the T-1B test. When flow was resumed after completion of plugging runs, instead of returning to its previous value, T1-5 module flow reached only 16.6 gpm and the module sum

reached 31.7 gpm. The flows were indicated at those values at the end of this report period.

At the end of calendar year 1969, the T-1B test assembly had accumulated a total of 1479.4 hr of steady-state testing at conditions of 750°F bulk sodium temperature, and 140-psi test section ΔP . At the request of BNW/FFTF, the test will be continued into February 1970, resulting in a probable total test duration of more than 2400 hr.

D. FFTF FLOW DUCT/RECEPTACLE EROSION-FLOW TEST (T-1D)

FFTF test T-1D is a follow-on test to T-1B to investigate the effect of a higher sodium velocity and to measure seat leakage at several seat loads. The test assembly consists of two modules, one having a flow duct diametral clearance of 0.035 in. and the other 0.045 in. Flowrate measurements will be made of only the 0.035-in.-clearance module; flow from the other module will be blocked. Seat leakage measurements will be made of both modules.

It is planned that, upon receipt and inspection of the T-1D assembly, test T-1B will be terminated, removed from the test loop, and the T-1D assembly will be installed. Testing is expected to start in March of 1970 and continue into October. All testing will be with a bulk sodium temperature of 750°F. Steady-state test periods, with a ΔP of 140 psi across the test article, will be preceded and followed by module flowrate and seat leakage measurements under specified conditions of ΔP and seat load. During steady-state testing, flowrate measurements will be recorded periodically on each shift and seat leakage will be measured every 90 to 100 hr.

X. MAINTENANCE AND MALFUNCTION ANALYSES

(LMFBR Program Plan 2-1.7, 2-1.9, 3-2.1.3, 3-2.2, 3-2.4, 3-4.3)
C. W. Griffin

A. INTRODUCTION

Incident reports received from the LMEC, PNL, ORNL, PRRR, INC, and ANL were reviewed. The reporting period is from August 1968 through November 1969. The results of this review are tabulated in a bar chart format to permit a general evaluation with respect to facility type, system, component, cause, and general systems effect as classified in Volume I of LMEC-Memo-69-7.*

Facility types were categorized with respect to the primary coolant used, since the respective environmental conditions differ greatly. The selected breakdown was Liquid-Metal-and Molten-Salt-Cooled Facilities, Water-Cooled Facilities, and Gas-Cooled Facilities. Component and systems failure events were depicted separately for each type.

If the incident was caused by or resulted in a component failure, the event was listed under component failures. If, however, the incident was caused by a systems inadequacy or malfunction unrelated to a component failure, the event was listed under system failures.

B. LIQUID-METAL- AND MOLTEN-SALT-COOLED FACILITY COMPONENT FAILURE EVENTS (Figure 81)

The "Component Failures" section of the chart depicts the total number of component-type failure event reports received by the LMEC from the facility types indicated. Since ANL and ORNL incident reports have only recently begun to arrive at the LMEC, the total number of events occurring under the "Nuclear Power Reactor" and "Nuclear Test Reactor" classifications is considerably less than that of "Component Test Facility" and "Component Test Loop" which represent primarily the events occurring at in-house LMEC facilities.

The "Systems" section of the chart shows the number of events, from all facility types, occurring in each system. The largest number of events in this

*"Failure Data Handbook for Nuclear Power Facilities," LMEC-Memo-69-7, Volume I, "Failure Data and Applications Technology" (August 15, 1969)

Figure 81. Liquid-Metal- and Molten-Salt-Cooled Facility
Component Failure Events (Sheet 1 of 2)

Figure 81. Liquid-Metal- and Molten-Salt-Cooled Facility
Component Failure Events (Sheet 2 of 2)

category occurred in the "Heat Transfer" system, which indicates difficulty with components in the primary and secondary coolant loops. This merely verifies the need for the RDT liquid metal component development program currently under way.

The second largest number of failure events occurred in the "Instrumentation and Control" system. Although some of these events were associated with specialized instrumentation required in liquid metal systems, the majority of the failure events occurred in conventional instrumentation and indicates that additional study of this system should be considered to determine what is necessary to upgrade performance. A similar case could be made for the "Transmission and Switchgear" system.

The "Components" section of the chart lists the total number of component failure events by component type. "Valves," "Pumps and Supports," and "Piping and Associated Fittings" represent the component failures associated with the "Heat Transfer" system and have the highest failure frequency of any component in that system. "Controllers" have by far the highest failure frequency in the "Instrumentation and Control" system and point specifically to an area where relative upgrading of preventive maintenance and applications engineering in that system should be considered. Again, a similar statement could be made where "Power Switchgear, Circuit Breakers, Relays, and Transformers" are concerned.

The "Cause" section of the chart classifies the number of events associated with each cause. As can be observed, the most frequent cause is "Unknown-General." This is unfortunate since, if the cause of failure is "unknown," the chances of a "repair" being truly successful is doubtful. In some cases, not establishing the cause of failure is justifiable; however, if the event becomes repetitious, a more careful engineering analysis and evaluation should be considered.

The "Effect" section of the chart shows that the majority of events reported resulted in a plant availability loss. Failure events were classified as resulting in "Plant Availability Loss" when it interrupted plant operation or, if the plant was shut down for other reasons, it would have prevented startup of the facility. Because a plant availability loss is costly both in terms of labor and materials

and scheduling delays, these types of failures deserve special attention and every effort should be extended to minimize them.

C. LIQUID-METAL- AND MOLTEN-SALT-COOLED FACILITY SYSTEM FAILURES EVENTS (Figure 82)

These charts show that the "Transmission and Switchgear" system has the highest failure frequency reported. However, no transmission and switchgear components were involved in the events and "Electrical-Transients" are the most frequent cause of these failures. The majority of "Transmission and Switchgear" system failures were caused by a loss of normal plant electrical power. Responsibility for this type failure rests with the electrical utility company that provides the plant power rather than the affected facility.

The remainder of the events resulted from human error type failures and are fairly evenly divided among operations, maintenance, installation, and design errors without any single category exhibiting an unusually high relative frequency.

D. GAS-COOLED FACILITY COMPONENT FAILURE EVENTS (Figure 83)

The facilities in this classification consist primarily of small research reactors, and only a very small number of incident reports have been received at the LMEC from this type of plant. It should be noted, however, that the "Instrumentation and Control" system has the highest relative failure frequency and "Controllers" represent the component with the highest failure frequency. As will be noted later, this component has an exceptionally high relative failure frequency in all three plant types.

The major cause of failure is "Unknown-General," and "Plant Availability Loss" represents 100% of the general system or component effect. The plant availability loss, in most cases, lasted only for a few minutes.

E. GAS-COOLED FACILITY SYSTEM FAILURE EVENTS (Figure 84)

Only three events from gas-cooled facilities were classified as system-type failures.

The events involved changes in core reactivity level and the exceeding of a plant operating limit. The cause of these events was classified as "Supervisory-Management Error" since procedural deficiencies were involved. Each event

Figure 82. Liquid-Metal- and Molten-Salt-Cooled Facility System Failure Events

Figure 83. Gas-Cooled Facility Component Failure Events

Figure 84. Gas-Cooled Facility System Failure Events

was thoroughly investigated by management at the facility involved, and recurrence would not be expected.

F. WATER-COOLED FACILITY COMPONENT FAILURE EVENTS (Figure 85)

The 175 component failure event reports received at the LMEC for the water-cooled facilities are shown. The "Instrumentation and Control" system and the "Reactor Equipment" systems have the highest failure frequency with the "Heat Transfer" system and the "Station Service Equipment" systems running third and fourth, respectively. A look at these charts reveals that "Controllers" are primarily responsible for observed relatively high failure frequency in the "Instrumentation and Control" system. "Irradiation Test Capsules and Specimens" are the components most responsible for the high failure frequency in the "Reactor Equipment" systems. No single component can be isolated as the major contributor to the relatively high failure frequency in the "Heat Transfer" system. In the "Station Service Equipment" system, the responsible component can be quickly identified as "Engines and Turbines (internal combustion)," which in this case refers to diesel engines in emergency electrical generating equipment.

The primary cause of failure, is "Unknown-General," and the general system or component effect with the highest relative frequency of occurrence is "Plant Availability Loss."

The comparatively high failure frequency associated with "Irradiation Test Capsules and Specimens" and "Engines and Turbines (internal combustion)" appears to be peculiar to the water-cooled facilities (possibly due to greater usage). A recent article in *Power*^{*} on the reliability of prime movers indicates that the failure frequencies encountered with diesel engines are excessive and that an additional effort to correct the difficulties with this component should prove successful.

Some of the "Irradiation Test Capsules and Specimens" failures were the responsibility of the customer who supplied the test device; this would suggest that better customer engineering, plant operations, and quality assurance

^{*}R. W. Parisian, "How Reliable are Today's Prime Movers?" *Power* (McGraw-Hill, Hightstown, New Jersey, Volume 114, January 1970)

Figure 85. Water-Cooled Facility Component Failure Events (Sheet 1 of 2)

Figure 85. Water-Cooled Facility Component Failure Events (Sheet 2 of 2)

interfacing could result in reduced failure frequencies with respect to this component. Larson and Darmohray* have reported difficulties encountered with tests at the General Electric Vallecitos Test Reactor, and the approaches used to improve plant availability are suggested reading.

Relatively high "Controller" failure frequencies are common to all plants reviewed and may indicate an area where some fundamental study and engineering analysis is required.

The large number of "Unknown-General" causes of failures suggests that additional effort to establish basic cause could be expected to improve plant availability and reduce labor and material loss through improved corrective action (i. e., final corrective action).

G. WATER-COOLED FACILITY SYSTEM FAILURE EVENTS (Figure 86)

These charts show "Irradiation Test Capsules and Specimens" and "Controllers" to be again the components associated with the highest relative failure frequencies. For these events, however, the cause of failure, in the majority of cases, is human error. "Plant Availability Loss" is again the effect with the highest relative frequency.

Much of that discussed under component failures will also apply to the system failures. Additional operator and maintenance training, procedure improvements, and system design changes represent some of the possible solutions to problems in the area.

H. RESEARCH LABORATORY FAILURE EVENTS (Figure 87 and Table 19)

Research laboratory failure events have been classified and tabulated in Table 19 and Figure 87. The tabulated events occurring in laboratories indicate that the majority of incidents are attributable to human error. Several cases of inadequate procedures or incomplete communication resulted in the spread of radioactive contamination, which if not promptly detected could have seriously affected the health of personnel.

All personnel involved in an operation involving radiation or radioactive materials must be thoroughly instructed in the steps involved, the reasons why

*H. J. Larson and G. F. Darmohray, "Scram Reduction at the General Electric Test Reactor," presented at the ANS Conference on Reactor Operating Experience, Atlantic City, New Jersey, July 23-26, 1967

Figure 86. Water-Cooled Facility System
Failure Events (Sheet 1 of 2)

Figure 86. Water-Cooled Facility System
Failure Events (Sheet 2 of 2)

Figure 87. Research Laboratory Failure Events

TABLE 19
RESEARCH LABORATORY INCIDENTS

Item	Component Associated with Events	Cause	Effect
1	Filter	Unknown - General	Potential injury to personnel (radioactive contamination)
2	Fuel	Human Error - Operations Error (Unintentional Action)	Potential injury to personnel (radioactive contamination)
3	Fuel	Human Error - Maintenance Repair (Adjustment/Setting Error)	Potential injury to personnel (radioactive contamination)
4	Fuel	Human Error - Supervisory or Management (Instructions)	Potential injury to personnel (radioactive contamination)
5	Tubing	Human Error - Supervisory or Management (Procedure-Posting)	Labor and material loss only
6	Valve	Environmental - Fluid Release	Potential injury to personnel (radioactive contamination)
7	Valve	Human Error - Operations Error (Failed to Close Valve)	Potential injury to personnel (overexposure-radiation)
8	Wiring and Connectors	Unknown - General	System/component inoperative (electrical short and sodium leak)
9	X-ray Equipment	Human Error - Operations Error (Failed to Turn Off)	Potential injury to personnel (overexposure-radiation)

the operation must be performed as specified, the consequences of foreseeable errors, and corrective action to be taken in the event of error. As a minimum, this will require review of the written procedure and hazard analysis by all personnel involved in the operation.

I. SUMMARY AND CONCLUSIONS

Evaluation of the failure data frequency charts indicates the following trends which should receive special consideration:

- 1) Liquid-Metal-and Molten-Salt-Cooled Facilities — The "Heat Transfer" system components have the highest relative failure frequency. The development program currently under way at the LMEC and other facilities should correct this problem. High relative failure frequencies in the "Transmission and Switchgear" system (circuit breakers, relays, etc.) should be given special facility attention.
- 2) Gas-Cooled Facilities — The number of failure events at gas-cooled facilities was too small to establish any trends; however, "controllers" were the component with the highest failure frequency.
- 3) Water-Cooled Facilities — "Irradiation Test Capsules and Specimens" and "Engines and Turbines (internal combustion)" were components with unusually high failure frequencies peculiar to these facilities. Additional effort, along the lines previously suggested to improve the performance of these components, could be expected to result in an increased plant availability.
- 4) Failure Events Common to All Facilities — "Controllers" were the single component which exhibited a high relative failure frequency at all facilities reviewed and may indicate an area where fundamental study and systems engineering analysis is required.

"Unknown-General" had the highest relative failure frequency of any cause, and this situation was found to be common to all facilities. This trend indicates that additional effort is required to establish basic cause if a significant reduction in repetitive-type failures is to be accomplished.

The objective of this incident summary report is to present a preliminary categorization of reported events with the minimum breakdown necessary to indicate important trends. It is hoped that the implications indicated by these trends can be utilized by the contributing facilities to improve quality in plant operations, maintenance, safety, and availability. It is recognized that the limited number of events, size of component categories, and the lack of an intimate familiarity with some of the facilities reviewed will limit, to some extent, the validity of some of the conclusions which could be reached by examination of the failure charts. However, it is believed that the components and causes singled out for special consideration in this summary represent "real" problem areas because of the very large deviations in relative frequency of failure occurrences between these and the other plant components.

It is also recognized, through LMEC experience in the preparation of the Failure Data Handbook (LMEC-Memo-69-7), that a further breakdown into categories of subsystem, component part, etc., would help to better define specific aspects of the failure events; this is expected to be done in the near future.

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