Liquid Metal Engineering Center (LMEC) Semiannual Technical Progress Report

January-June 1971

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INTRODUCTION

The overall objective of the LMEC is to provide optimum operation and management to implement 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 and coordination 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.

The immediate purpose of the LMEC is to support the FFTF program by providing sodium component and instrumentation testing facilities and by supplying detailed technical assistance to the FFTF development program. In this regard, the LMEC concluded its technical management activities on FFTF IHX's, pumps, and valves. During this report period, responsibility for technical management of associated contracts has been transferred to WADCO.

A further objective is to provide technical assistance and testing capability for LMFBR requirements other than the FFTF program, as directed.

Only those projects which, during this reporting period, have generated direct technical progress information of immediate use and interest to the LMFBR community are reported herein. Those projects identified as being administrative, general support, or contractor management in scope, or which have generated limited technical information, are not included.

SUMMARY

PRECISION MECHANISMS IN SODIUM (PMIS)

The 6-month hold test of the Control Rod Drive Mechanism (CRDM) roller nut over 1050°F sodium was successfully completed when the mechanism scrambled and cycled 10 times. The mechanism was disassembled and inspected. Sodium blockage occurred between the leadscrew and guide bushing; however, the scram time was not affected. The CRDM with control console was returned to Todd Shipyards.

Dynamic testing of the In-Vessel Handling Machine (IVHM) guide roller was successfully completed in 450°F sodium with a test load of 1190 lb. A long-term dwell test of the mechanism in 1100°F sodium was initiated. High-purity sodium (<10 ppm O_2) is being used for the test.

MATERIALS COMPATIBILITY IN SODIUM (MCIS)

Additional friction and wear tests in direct support of FFTF were performed on prototypical in-core configurations. The current series of tests (Test Report T-8, Addendum 3) involves short-stroke oscillatory motion in contrast to the rotary unidirectional motion used in previous tests (Addenda 1 and 2). During the reporting period, six tests were completed involving candidate material combinations for duct wear-pad applications in FFTF fuel and control elements, and three were initiated involving wire wrap/clad applications in FFTF fuel rods.

Three of the duct wear-pad tests involved 20% cold-worked (CW) Type 316 SS rubbing against itself at 1200°F under a contact pressure of 300 psi, with stroke length based upon d , the pin diameter, as the variable; test specimens with $<d/2$ stroke had approximately half the pin wear experienced during the $d/2$ and $>d/2$ strokes. While the wear in the 20% CW Type 316 SS pin/20% CW Type 316 SS plate test was not excessive, the apparent friction values encountered at 1200°F ultimately became so high that this material combination appears to be unacceptable for the wear-pad applications as presently designed.

The next series of pin/plate tests involved Inconel 718 pins rubbing against 20% CW Type 316 SS plates at 1160°F with contact pressure as the primary

variable. Oxygen content in this series of tests was less than 5 ppm, compared to 5 to 10 ppm in the previous tests. Preliminary analysis indicates that the frictions are well above the 0.25 upper limit desired.

In all of the pin/plate tests completed during the reporting period, friction measurements also were made at temperatures down to 450°F to obtain additional data on the effects of temperature and oxygen content on the behavior of a lubricative sodium-chromium-oxygen complex ($\text{Na}_x\text{Cr}_y\text{O}_z$) which forms on the surface of austenitic stainless steel.

Rubbing in the three tests involving 20% CW Type 316 SS wire wrap vs 20% CW Type 316 SS cladding was started in 1160°F sodium near the end of the reporting period. Contact load was the primary variable and initial measurements showed "apparent" friction values between 5 and 10 for the 1.5-lb load, half that high for the 5-lb load, and between 1 and 2 for the 20-lb load. The data indicate that the "apparent" friction values are based upon the shear stress required to machine away metal on a macro scale, rather than on a micro scale, due to the Hertzian stresses involved in the crossed-cylinder geometry. Similar tests in ambient air revealed that during the first few oscillatory strokes, actual machining occurred until a suitable sized flat contact area between wire wrap and clad was established.

RESULTS OF HALLAM IHX EXAMINATION

Idaho Nuclear Corporation (INC) completed a destruction examination of one of the six intermediate heat exchangers from the Hallam Nuclear Power Facility (HNPF). The primary objective of the examination was to assess crevice corrosion problems and evaluate operational deterioration associated with front-face-type, tube-to-tubesheet welded joints.

Metallurgical evaluation of the welds found them to be structurally sound. The examination revealed stress corrosion cracking in some tubes and ligaments of the top tubesheet, but not in the bottom tubesheet. No other evidence of operational damage was found.

SPTF CONTROL VALVE WATER CAVITATION TESTS

Water cavitation tests were performed on two Mosser Industries 18-in. butterfly valves designed to simulate the geometry of SPTF main flow loop

butterfly valves. Tests have been completed to determine the head loss coefficients, six-pipe-diameter valve spacing hydraulic interference effects, incipient cavitation index, and cavitation erosion damage effects. In addition, a single test was conducted to investigate the upstream pressure effect on the incipient cavitation index that had been reported by other investigators.

Test results have revealed the following:

- 1) The head loss coefficient determined from the tests differed slightly from that predicted by theory; the coefficient was lower at smaller valve openings and higher at larger valve openings.
- 2) Hydraulic interference resulting because of the six-pipe-diameter spacing between the valves was not in evidence.
- 3) The incipient cavitation index determined in the above tests was higher than that used by the LMFBF development contractor (Crane Co.) for the SPTF main flow loop valves. The test results indicate that additional pressure reducing devices will be required if the SPTF is to be operated free of cavitation.
- 4) Difficulties were experienced with the damage detector in conducting the cavitation erosion damage tests; and uncertainties related to test duration requirements, damage location, and the interpretation of damage effects have resulted in a decision to terminate further damage tests.
- 5) Results of the initial test to determine upstream pressure effects on the incipient cavitation index have indicated that the incipient cavitation index can be expected to increase with increased upstream pressure. Further tests are being planned to evaluate this phenomenon.

PUMP SEAL TEST PROGRAM

A pump shaft seal of the design presently in service at the Enrico Fermi Atomic Power Plant (EFAPP) was operated in the Pump Seal Test Facility (PSTF). It was the purpose of the overall program to use the seal as the basis for qualifying the PSTF for testing shaft seals. Highlights of seal performance data and seal oil/sodium vapor interaction are presented in Section IV.

The Fermi seal was operated in PSTF No. 2, which is designated as the oil-type seal test facility. During facility qualification, PSTF No. 1 was used to provide the sodium environment for the FFTF prototype control rod drive mechanism (CRDM). The sodium system of PSTF No. 1, therefore, was qualified by the CRDM test program. Seal drive and support hardware are functionally identical. Operation of drive No. 2 without difficulty is considered an acceptable confirmation of the drive and support assembly for PSTF No. 1.

The PSTF has been operated satisfactorily with sodium at 1200°F and a cover gas pressure of 50 psig. Cold trapping has succeeded in keeping oxide levels below 10 ppm under normal conditions. Impurity monitoring utilized samples obtained from forced-flow sample stations. Utilizing the qualification program, system deficiencies were detected and improvements and repairs were made which would have delayed critical LMFBR seal tests if not completed early.

Through the application of Fermi-type seal operating experience with halocarbon oil (Fluorolube), LMEC has been able to demonstrate undesirable sodium system contamination. With various oils presently being evaluated for use in future LMFBR pump shaft seals, the undesirable experience with Fluorolube, both at LMEC and at Fermi, has provided valuable input to LMEC's recommendation that Fluorolube not be used.

INSTRUMENTATION TESTING, EVALUATION, CALIBRATION, AND STANDARDS

A test program was performed to provide information relevant to the design of sodium-to-atmosphere leak detection systems for use in the FFTF primary system. This program, which was limited in scope, provided performance data on several selected detectors.

Phase I of the high-temperature strain gage development program was continued by Battelle Memorial Institute. Tests of selected candidate wire materials have established two alloys with characteristics suitable for strain gage application at 1200°F. These alloys were successfully drawn to 0.001 and 0.002-in.-diameter wire.

The high-temperature pressure transducer development program conducted by Bailey Meter Company was suspended in February because of funding limitations. This program will resume July 1, 1971.

Tests were performed to determine the feasibility of determining the response time of large permanent magnet flowmeters under simulated conditions. These tests entailed oscillating aluminum cylinders in the magnetic field of a permanent magnet with a 13-in. magnetic gap. Preliminary results indicated that the technique appears feasible and additional tests should be performed on a higher sophisticated level.

Sodium tests of the Transit Time Flowmeter were completed. Selected results of these tests, which were performed at the SCTI facility, are presented in Section VI in addition to results of previously performed water tests.

CHEMICAL AND METALLURGICAL LABORATORY

Trace metal and oxygen analyses of EBR-II sodium received from ANL for the sample interchange program has been completed. The techniques used were those described in ANL/ST-6 (to be issued as RDT-Std-F 3-40T). The results have been reported for discussion at the next ANL meeting of the National Standards Working Group.

Evidence acquired by observation and experimentation indicates that the oxygen content problem with MCIS test vessel No. 2 sodium is due to metal particles. Surveillance analysis data indicate a general, direct correlation between particulate content and apparent oxygen concentration.

Sodium compatibility selection tests are being conducted with various commercial insulation materials for 1000° to 1350°F service. The test technique involves heating about 5 gm of sodium to approximately 700° to 1000°F and pouring in air onto pre-dry insulation.

Laboratory capability has been considerably broadened during this report period. This capability will continue to grow during the next semiannual period as recently ordered equipment is installed. The purpose is twofold: (1) accommodate ANL/ST-6 methods for the characterization of sodium and cover gases; and (2) support facility construction, modification, and surveillance efforts attendant to various LMEC programs.

A defective Wm. Powell 1-1/2 in. gate valve body, Type CF 8M stainless steel, was sectioned and metallurgically examined. Of major interest were three defective areas which showed single fracture defect depths up to 90% of the valve body wall.

Specimens of the initial procurement of stainless steel pipe purchased to RDT Standard RDT M 3-3T were metallographically examined; laps or seams of up to 0.015 in. were discovered. A report summarizing the defects was transmitted on June 18, 1971.

Sections from the failed LCTL core tank (T-1) lid were examined. Predominant intergranular fractures, with some branching transgranular mode in localized areas, were observed.

LCTL/SCTL OPERATION, MAINTENANCE, AND MODIFICATION

A scheduled inspection of the LCTL core tank lid in April 1971 revealed numerous radial cracks and corrosion pits in the dished head. The incident was formally reported and a detailed failure analysis was conducted.

Results of metallographic, chemical, thermal, and stress analyses of the cracked lid have attributed the cause of failure to stress corrosion of Type 304 SS by corrodents and/or impurities leached from the vermiculite insulation by moisture collected in the recessed space below the rectangular opening of the lid cover plate. A thorough inspection of the core tank revealed no indications of cracks or corrosion, and it is concluded that the core tank material is in satisfactory condition.

This incident did not cause damage to the facility or injury to operating personnel.

CREEP RATCHETTING LABORATORY TESTS

Creep ratchet tests were performed at 1200° and 1100°F by cyclic reverse bending of Type 304 SS strips over mandrels with hold times of 24 hr (2 weeks for 1100°F tests) between bend cycles while the strip is subjected to continuous axial tension. Results of the tests indicate that Bree's charts yield a reasonable and conservative estimate of the amount of ratchet strain to be anticipated under these conditions. Present FRA 152-3 limits on deformation can apparently be reached in 20 cycles or less at reasonable stress levels.

I. PRECISION MECHANISMS IN SODIUM (PMIS)

(LMFBR Program Plan 3-3, 3-7.3, 3-10, 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, gears, fuel handling machine drives, 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.

B. CRDM ROLLER NUT TEST

The 6-month hold test over 1050°F sodium was completed on January 25, 1971, with the successful scrambling of the Control Rod Drive Mechanism (CRDM). The unlatching time, 0.082 sec, was the same as that recorded in the

past. The leadscrew drop time, 0.447 sec, was essentially the same as that recorded previously, 0.437 sec. The CRDM was operated for 10 additional run cycles and then the test was shut down. The test conditions for the extended dwell period are shown in Figure 1. Analyses of sodium and cover-gas samples are shown in Tables 1 and 2. The test assembly was purged periodically to reduce the contamination levels.

TABLE 1
CRDM SODIUM SAMPLES

Sample Number	Sample Date	Location	O ₂ Content (ppm)
CRDM-33	1-6-71	1050°F sodium pool	3.5, 3.2, 3.3

TABLE 2
CRDM ARGON GAS SAMPLES

Sample Number	Sample Date (1971)	Location	Impurities (ppm)			
			O ₂	H ₂	N ₂	CH ₄
GT-CRDM-163	1-5	Roller Nuts Elevation	203	6	2174	0.9
GT-CRDM-164	1-7		14	6	814	0.7
GT-CRDM-165	1-12		70	21	1120	2.8
GT-CRDM-166	1-14		60	35	1115	3.3
GT-CRDM-167	1-19		63	25	1167	2.7
GT-CRDM-168	1-21		68	31	1121	2.5
GT-CRDM-169	1-25		2	463	417	11.3

Disassembly of the CRDM was accomplished on February 2, 1971. Several observations were made during this operation (see Figures 2 through 7). A sodium blockage occurred between the leadscrew and the lower bushing assembly during the 6-month hold. The blockage explains the purging difficulties encountered during the test. The pattern of the purging difficulties indicates that the blockage occurred within 1 to 3 weeks from start of testing; however, the blockage did not change the scram time. In addition, the sound probe recording was

TEST TEMPERATURE: 1050°F

LMEC-34028

Figure 1. CRDM Test Assembly Thermal Profile During 6-Month Hold (Dwell) Period

LMEC-71-7

LMEC-P34003A

Figure 2. Sodium Deposits in Reworked Autoclave Housing as Removed from Test Vessel Prior to Fourth (Final) Inspection

LMEC-71-7

LMEC-P34010A

Figure 3. Sodium Deposit at Interface Between Reworked Autoclave Housing and Lower Bushing Assembly as Removed from Test Vessel Prior to Fourth (Final) Inspection

LMEC-P34009A

Figure 4. Sodium Blockage Between Leadscrew and Lower Bushing Assembly as Removed from Test Vessel Prior to Fourth (Final) Inspection

LMEC-71-7

LMEC-P34007A

Figure 5. Roller Nut Assemblies as Removed from Test Vessel Prior to Fourth
(Final) Inspection

LMEC-P34006A

Figure 6. Roller Nut Assembly as Removed from Test Vessel Prior to Fourth
(Final) Inspection

LMEC-P34005A

Figure 7. Metal Filings on Buffer Piston as Removed from Test Vessel Prior to Fourth (Final) Inspection

similar to that of past tests. The temperature of the lower bushing assembly was held at 180°F during the 6-month hold. In earlier tests, this temperature was held at 250°F. Evidently, the lower temperature and lack of CRDM movement caused sodium to be deposited from vapors rising in the drive line annulus.

The blockage plus the lack of CRDM movement accounts for the very clean appearance of the roller nuts, leadscrew, and other parts above the lower bushing assembly elevation. There was a very thin, light-gray sodium film observed on some of the parts accompanied by a few small sodium droplets. A few very fine metal filings from the spline shaft were adhering to the position indication magnets on the buffer piston and the rotor motion indication magnet. A few sodium droplets and a light layer of black wear particles were found in the bottom of the autoclave housing. The roller nuts, thrust bearing, radial bearing, and synchronizing bearing were free when turned by hand.

A sodium vapor deposition rate was obtained from a sample flag mounted in the vicinity of the roller nuts. All of the results to date are presented in Table 3. A summary of the CRDM travel data is given in Table 4.

The fourth (final) inspection was completed during the reporting period. A summary of the stator electrical resistance is given in Table 5. Table 6 presents a summary of the four CRDM inspections. The appearance of each individual part during the fourth (final) inspection was identical to that during the third inspection. There were virtually no dimensional changes in the parts between the two inspections.

Completion of the 6-month hold test concluded the CRDM roller nut test at the LMEC. The CRDM and control console were returned to Todd Shipyards on February 25, 1971. The adapter, simulated control rod weight, buffer spring assembly, and associated equipment were placed in storage at the LMEC.

C. IVHM DRIVE SCREW TESTS

The In-Vessel Handling Machine (IVHM) for FFTF will not use a ball-nut screw mechanism because of a conceptual design change incorporating a rising stem principle; therefore, testing of all ball-nut screws has been terminated. Test data for previous ball-nut screws operating in sodium is presented in the previous semiannual progress report (LMEC-71-1).

TABLE 3
CRDM SODIUM VAPOR DEPOSITION RATES

Sample Number	Exposure Period (date and time of day)		Time Conditions	Exposure Time (hr)	Deposition Rate ($\mu\text{g Na/cm}^2/\text{hr}$)
	Start	End			
VD-CRDM-1	1-5-70 (23:00)	1-7-70 (16:00)	600°F hold period and 10 cycles	41.0	0.1290*
VD-CRDM-2	1-9-70 (14:00)	1-12-70 (09:30)	600°F post hold period	67.5	0.0025
VD-CRDM-3	1-23-70 (10:25)	1-26-70 (09:10)	800°F cycling period	70.5	0.0011
VD-CRDM-4	1-27-70 (13:10)	2-2-70 (12:35)	800°F holding period	133.5	0.0010
VD-CRDM-5	2-4-70 (16:35)	2-6-70 (11:00)	1050°F cycling period	42.5	0.0030
VD-CRDM-6	2-9-70 (16:10)	2-13-70 (08:55)	1050°F holding period	88.5	0.0009
VD-CRDM-7	3-17-70 (16:00)	4-9-70 (10:10)	1050°F tests after second inspection	546.0	0.0002
VD-CRDM-8	7-9-70 (16:00)	2-1-71 (10:00)	1050°F 6-month hold period	4962.0	0.0009

*This first sample is believed to be inaccurate. Additional sodium may have been picked up by the sample tab during the removal operation.

TABLE 4
CRDM TRAVEL DATA

Test Temp (°F)	CRDM Environment	Individual Test Totals			Accumulative Totals		
		Cycles	Approx Travel (ft)(e)	Scrams	Cycles	Approx Travel (ft)(e)	Scrams
First (as received) Inspection							
500	Argon	10 (a)	61.2	0	10	61.2	0
600	Sodium Vapor + Argon	210(b,f)	1902.6	210	220	1963.8	210
800	Sodium Vapor + Argon	210(b,f)	1902.6	210	430	3866.4	420
1050	Sodium Vapor + Argon	210(b,f)	1905.6	211	640	5772.0	631
Second Inspection							
1050	Sodium Vapor + Argon	200(b,f)	1812.0	200	840	7584.0	831
1050	Sodium Vapor + Argon	200(b,f)	1815.0	201	1040	9399.0	1032
1050	Sodium Vapor + Argon	10(b)	91.8	10	1050	9490.8	1042
1050	Sodium Vapor + Argon	4(c)	12.2	4	1054	9503.0	1046
Third Inspection							
1050	Sodium Vapor + Argon	3(d)	2.5	3	1057	9505.5	1049
1050	Sodium Vapor + Argon	1(b)	9.2	1	1058	9514.7	1050
1050	Sodium Vapor + Argon	10(c)	30.2	10	1068	9544.9	1060
1050	Sodium Vapor + Argon	10(b,g)	91.8	10	1078	9636.7	1070
1050	Sodium Vapor + Argon	1(a,h)	6.1	2	1079	9642.8	1072
1050	Sodium Vapor + Argon	10(b,i)	91.8	10	1089	9734.6	1082

- (a) One cycle defined as: Withdraw 36-1/4 in. at 9 in. /min. Insert 36-1/4 in. at 9 in. /min.
- (b) One cycle defined as: Withdraw 36-1/4 in. at 9 in. /min. Insert 36-1/4 in. at 9 in. /min. Withdraw 36-1/4 in. at 9 in. /min. Scram.
- (c) One cycle defined as: Withdraw 36-1/4 in. at 9 in. /min. Scram.
- (d) One cycle defined as: Withdraw 10 in. at 9 in. /min. Scram.
- (e) Based only on those strokes where roller nuts are engaged (travel during scram strokes is not counted).
- (f) Includes 1-week hold period.
- (g) Includes 4-week hold period.
- (h) Includes 27-day hold period.
- (i) Includes 6-month hold period.

TABLE 5

CRDM ELECTRICAL RESISTANCE READINGS

Item		Readings			
		At First (as recd) Inspection	At Second Inspection	At Third Inspection	At Fourth (final) Inspection
		(megohms)			
Insulation resistance from windings to core*	A to B (J1A-J2A)	8,000	200,000	2,000,000	900,000
	A to C (J1A-J3C)	7,600	350,000	2,000,000	1,400,000
	B to C (J2A-J3C)	5,000	300,000	700,000	1,000,000
	A to J1A Ground	9,000	100,000	300,000	1,400,000
	B to J2A Ground	2,000	950	150,000	1,000,000
	C to J3C Ground	1,200	35,000	50,000	1,000,000
		(ohms)			
Three-phase winding resistance†	A ₂ to A (J1A-D)	6.2469	6.396	6.282	6.272
	AA ₂ to A (J1A-C)	6.2709	6.420	6.302	6.294
	B ₂ to B (J2A-B)	6.2409	6.391	6.275	6.265
	BB ₂ to B (J2A-C)	6.2679	6.416	6.301	6.295
	C ₂ to C (J3C-B)	6.2519	6.399	6.285	6.275
	CC ₂ to C (J3C-A)	6.2529	6.399	6.286	6.282

*100 megohms minimum.

†Readings corrected to 25 C. Corrected resistance shall be 6.5 ohms $\pm$ 2-1/2%.

TABLE 6

CRDM INSPECTION RESULTS
(Sheet 1 of 3)

Part Name	Area Inspected	Measurements (in.)			
		First (Initial) Inspection(a)	Second Inspection(b)	Third Inspection(c)	Fourth (Final) Inspection(g)
Leadscrew	Thread major diameter at 3 in. below latch area	1.5453	1.545	1.545	1.545
		1.5453	1.5448	1.545	1.545
	Thread minor diameter at 3 in. below latch area	1.2845	1.284	1.288	1.288
		1.2845	1.284	1.288	1.288
	Thread widths at 3 in. below latch area	0.1875	0.1875	0.1875	0.1875
		0.1875	0.1875	0.1875	0.1875
		0.1875	0.1875	0.1875	0.1875
	Thread major diameter at 33 in. below latch area	1.5453	1.5452	1.545	1.545
		1.5454	1.545	1.545	1.545
	Thread minor diameter at 33 in. below latch area	1.2845	1.284	1.286	1.286
		1.2845	1.284	1.286	1.286
	Thread widths at 33 in. below latch area	0.1875	0.1875	0.1875	0.1875
		0.1875	0.1875	0.1875	0.1875
		0.1875	0.1875	0.1875	0.1875
		Threads No. 23 & 24 from the top showed light latching marks.			
Leadscrew bushing (lower)	ID	1.5792	1.5791	1.579	1.5792
Leadscrew bushing (upper)	ID	Not inspected.		1.5582	1.5578- 1.5582
Segment arms	Pivot pin hole diameters	0.500	0.500	0.500	0.501
Segment arms pivot pins	OD	0.499	0.499	0.499	0.499
Roller nut inner races	Races (4)	No wear	Lower (loaded) races had wear.		
Roller nut W	Axial play	0.005 TIR ^(d)	0.0065 TIR	0.0065 TIR	0.005 TIR
	Bearing outer races	No wear	Lower (loaded) races had wear.		
	Teeth OD's	1.8102	1.8101	1.8102	1.8102
		1.8100	1.80996	1.8101	1.8101
1.8101		1.8101	1.8102	1.8102	

TABLE 6
CRDM INSPECTION RESULTS
(Sheet 2 of 3)

Part Name	Area Inspected	Measurements (in.)			
		First (Initial) Inspection(a)	Second Inspection(b)	Third Inspection(c)	Fourth (Final) Inspection(g)
Roller nut W (Continued)	Teeth root diameters	1.5424 1.5429	1.54280 1.54290	1.543 1.543	1.543 1.543
	Teeth widths	0.179 0.1785 0.179	0.1790 0.1785 0.1790	0.178 0.178 0.179	0.178 0.178 0.178
	Teeth profiles	Identical			
Roller nut X	Axial play	0.004 TIR	0.0068 TIR	0.007 TIR	0.006 TIR
	Bearing outer races	No wear.	Lower (loaded) races had wear.		
	Teeth OD's	1.8102	1.8104	1.8102	1.8102
		1.8101	1.8102	1.8102	1.8102
		1.8102	1.81035	1.8103	1.8103
	Teeth root diameters	1.5428	1.5432	1.5435	1.5435
		1.5430	1.54405	1.544	1.5439
	Teeth widths	0.179	0.1785	0.178	0.178
		0.179	0.1785	0.178	0.178
		0.179	0.1787	0.178	0.178
Roller nut Y	Teeth profiles	Identical			
	Axial play	0.0043 TIR	0.0045 TIR	0.0075 TIR	0.0075 TIR
	Bearing outer races	No wear.	Lower (loaded) races had wear.		
	Teeth OD's	1.8104	1.8104	1.8104	1.8104
		1.8103	1.81035	1.8104	1.8104
		1.8105	1.8105	1.8106	1.8106
	Teeth root diameters	1.5429	1.54290	1.544	1.5435
		1.5434	1.54335	1.5435	1.5435
	Teeth widths	0.179	0.1785	0.178	0.178
		0.179	0.1789	0.178	0.178
		0.179	0.1789	0.178	0.178
Roller nut Z	Teeth profiles	Identical			
	Axial play	0.0043 TIR	0.0044 TIR	0.005 TIR	0.008 TIR
	Bearing outer races	No wear.	Lower (loaded) races had wear.		
	Teeth OD's	1.8102	1.8103	1.8102	1.8103
		1.8101	1.8103	1.8102	1.8102
		1.8103	1.8104	1.8103	1.8103

TABLE 6

CRDM INSPECTION RESULTS
(Sheet 3 of 3)

Part Name	Area Inspected	Measurements (in.)			
		First (Initial) Inspection(a)	Second Inspection(b)	Third Inspection(c)	Fourth (Final) Inspection(g)
Roller nut Z (Continued)	Teeth root diameters	1.5445	1.5448	1.545	1.545
		1.5449	1.5452	1.545	1.545
	Teeth widths	0.179	0.1785	0.178	0.1775
		0.179	0.1790	0.178	0.1778
		0.1792	0.1790	0.178	0.1779
	Teeth profiles	Identical			
Thrust bearing	Radial play	0.0033 TIR	0.0033 TIR	0.0033 TIR	0.0033 TIR
	Axial play	0.0028 TIR	0.0032 TIR	0.005 TIR	0.005 TIR
Radial bearing	Radial play	0.0006 TIR	0.0006 TIR	0.001 TIR	0.001 TIR
	Axial play	0.0045 TIR	0.0038 TIR	0.0026 TIR	0.010 TIR
Synchronizing bearing	Radial play	0.0007 TIR	0.0012 TIR	0.0007 TIR	0.0007 TIR
	Axial play	0.0045 TIR	0.0046 TIR	0.0036 TIR	0.004 TIR
Internal spline bushing	Internal spline	No wear	Chatter marks on thrust side of spline.		
Spline shaft	Spline OD	0.9739	0.9745	0.9744	0.9744
		0.9748	0.9747	0.9746	0.9746
			(e)	(f)	

(a) 10-15-69, 10-16-69, 10-17-69, 10-28-69, 10-30-69, 11-7-69, 12-8-69.

(b) 2-18-70, 2-25-70, 3-2-70.

(c) 4-14-70, 4-15-70, 4-17-70, 4-20-70, 4-21-70, 4-22-70.

(d) TIR = total indicator reading.

(e) Heavy galled area on thrust side of spline for 38.9 in. starting 7.375 in.
from bottom end of shaft.(f) Light galled area on thrust side of spline for 38.9 in. starting 7.375 in. from
bottom end of shaft.

(g) 2-2-71, 2-5-71

D. IVHM GUIDE ROLLER TEST

Testing of the IVHM guide roller mechanism in 450°F sodium was initiated on April 15, 1971. Four test rollers and tracks were installed in the test fixture as shown in Figures 8 and 9. A schematic of the mechanism installed in the sodium test vessel and photographs of the drive installation and instrumentation are shown in Figures 10, 11, and 12.

The mechanism successfully completed 26,270 cycles in 450°F sodium at a test load of 1190 lb. A summary of the test conditions is presented in Table 7. A cycle is 34 in. of travel (17 in. up and 17 in. down). The drive force varied from 1250 to 1350 lb over the entire test period. This variation in drive force (± 50 lb in 1300) was probably caused by variations in the friction associated with the roller mechanism and the drive system (seals, bearings, etc.)

LMEC-P42005A

Figure 8. IVHM Guide Roller Test Rollers and Tracks
(Typical of one side)

LMEC-71-7

LMEC-P42003A

Figure 9. IVHM Guide Roller Lateral Support Bearings

LMEC-71-7

LMEC-34029

Figure 10. IVHM Guide Roller Test Schematic

LMEC-P35004A

Figure 11. IVHM Guide Roller Drive Mechanism

LMEC-71-7

LMEC-P35005

Figure 12. Control and Data Acquisition Console for IVHM Guide Roller Test

LMEC-71-7

TABLE 7

IVHM GUIDE ROLLER FIRST DYNAMIC TEST CONDITIONS

Item	Range of Values
1. Sodium level (in. below cover)	36.0 $\pm$ 0.5
2. Sodium temperatures ($^{\circ}$ F):	
a. Upper section of test guide	435-452
b. Lower section of test guide	435-452
3. Fixture temperature ($^{\circ}$ F):	
a. Drive shaft penetration at cover	55-75
b. Drive shaft seal	60-85
c. Weight adjustment shaft seal	62-85
d. Vessel cover seal	60-90
4. Cover gas:	
a. Pressure (in. of H ₂ O)	4.8-7.8
b. Flow (scfh)	0.2
5. Test article load (lb)	1190 + carriage weight
6. Total cycles	26,270
7. Drive force (lb)	1250 to 1350

Results of chemical analyses of sodium samples obtained from a forced-flow sodium sampler on the side of the test vessel are shown in Table 8. During the test period, the sodium conformed to the test requirements of <10 ppm oxygen and <40 ppm carbon impurities.

On May 3, 1971, the guide roller mechanism was returned to Atomics International (AI) for detailed examination of the bearing surfaces, prior to any further testing. Inspection by AI revealed no significant wear. Minute indentations were observed on the surfaces of rollers and races of all test rollers. Their cause has not been determined.

The test article was returned to the LMEC and testing continued on May 27 with the mechanism successfully completing 1000 cycles in 440 $^{\circ}$ F sodium with

TABLE 8

IVHM GUIDE ROLLER FIRST DYNAMIC TEST
SODIUM ANALYSES

Sample Date	LMEC Lab No.	Impurities (ppm)			
		O ₂	C	Ca	K
4-8-71	485	13.7, 20.7	7.3, 9.0	<1	40
4-9-71	487	14.2, 15.1, 11.9	-	-	-
4-13-71	490	12.9, 10.2	-	-	-
4-15-71	493	8.3, 11.0	-	-	-
4-22-71	496	8.6, 10.8	11.0, 12.0	-	-

a test load of 1190 lb. During this test operation, the drive force varied between 1200 and 1260 lb. The sodium impurity levels during this test are shown in Table 9.

The sodium was then heated in preparation for the long-term dwell at 1100°F. Since this was the first time the test vessel was heated above 450°F, the oxygen impurity level in the sodium increased significantly as shown in Table 10 (sample dates June 2 and 3, 1971). On June 7, the system was hot-drained at 1100°F into the drain tank at 600°F and the vessel was refilled with 600°F sodium which was then reheated to 1100°F. However, sodium samples taken on June 10 and 15 (see Table 10) showed the oxygen content to be in the 60-ppm range. Therefore, on June 17 the system was again hot-drained at 1100°F; however, this time the sodium was allowed to cool down to 270°F in the drain tank. In this manner, the dump tank was used as a large cold trap by allowing the oxides and particulates in suspension in the sodium to settle out over a period of about 70 hr. The test vessel was then refilled with sodium and samples were taken at various temperature levels (sample dates 6-22, 6-23, 6-25, and 6-28 in Table 10) up to 1100°F. The thermal convection cold trap on the side of the test vessel was operated at 250 to 270°F during this time. By means of this technique, the oxygen content in the sodium was reduced to <5 ppm. In conformance with the test requirements, the hot trap was turned on and, as a result, the oxygen content increased to about 15 ppm. After a day of cold trapping, the oxygen content was reduced to about 6 ppm (sample date 7-2 in Table 10) and the dwell test was initiated at this time. During this period, the carbon content remained at about 5 ppm, which is in conformance with the test requirement of <20 ppm.

TABLE 9

IVHM GUIDE ROLLER SECOND DYNAMIC TEST
SODIUM ANALYSES

Sample Date	LMEC Lab. No.	Impurities (ppm)	
		O ₂	C
5-19-71	523	11.3, 13.2	5,8
5-21-71	529	12.6, 12.0	-
5-26-71	537	11.4, 8.9	-

TABLE 10

IVHM GUIDE ROLLER DWELL TEST SODIUM ANALYSES

Sample Date (1971)	LMEC Lab. No.	Sodium Temperature (°F)	Impurities (ppm)		
			O ₂	C	wt % Particulates
6-2	546	900	34.6, 19.1	<5, <5	-
6-3	549	1100	45.0, 45.3	-	-
6-10	561	1100	72.4, 59.1	<5, 8	-
6-15	570	1100	78.7, 40.7	-	-
6-22	576	270	12.9, 12.1	-	0.003
6-23	579	450	3.5, 3.2	-	<0.001
6-25	583	900	3.2, 3.3	-	-
6-28	587	1100	3.9, 4.6	5, 5	-
7-1	590	1100 (1230°F hot trap temp.)	17.9, 15.1, 13.4	-	-
7-2	591	1100 (1250°F hot trap temp.)	5.8, 5.7, 6.3	-	-

The dwell test is being conducted with a load of 295 lb and will continue for 15 days, at which time the sodium temperature will be lowered to 450°F and the mechanism cycled for 200 times. The sodium temperature will again be raised to 1100°F and the mechanism allowed to dwell for 87 days, at which time the sodium temperature will again be lowered to 450°F and the dynamic testing initially performed on the mechanism will be repeated. The mechanism will then be removed from the test vessel and will undergo detailed examination.

II. MATERIALS COMPATIBILITY IN SODIUM (MCIS)

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

J. J. Droher

A. INTRODUCTION

The primary objectives of the MCIS project during the reporting period were as follows:

- 1) To obtain quantitative data on friction and wear in sodium of duct-pad candidate materials under prototypical FFTF conditions
- 2) To obtain quantitative data on friction and wear of fuel cladding undergoing rubbing by wire-wrap under prototypical FFTF conditions.

The friction and wear data obtained during this reporting period involved short-stroke, oscillatory motion only. The oxygen content of the sodium during these tests was ≤ 5 ppm. The rubbing surfaces on the duct-pad candidate materials were flat-ended pins and plates. Wire-wrap/cladding tests used actual wire wrap and cladding proposed for the FFTF fuel rods. In all of the oscillatory tests, loading was by direct dead weights. In each case, the rubbing surface or surfaces on the weighted, driven plate were supported by three rubbing surfaces, equally spaced from the center of rotation and 120 degrees apart. Sliding motion was converted from the rotating motion of the drive motor to very short, reciprocating sliding contact prototypical of certain FFTF component operations. This was accomplished by using the concept of a bell-crank (see Figure 13) to convert unidirectional rotation of a shaft to rotation of a few degrees back and forth. The extent of the rotation could be set anywhere from 3 to 15 degrees by means of a screw adjustment external to the apparatus. An indicating device was included that showed the actual angle of rotation of the moving wear surface immersed in the sodium.

The apparatus was designed to take any thickness or configuration of wear surface, up to 5 in. in any dimension. The length of stroke is controllable to ± 0.001 in. from 0.005 to 0.500 in. During each stroke, the wear specimen builds up to a maximum velocity, dies away to zero velocity, and then is motionless for a fraction of a second prior to stroke reversal. The maximum velocity is adjustable from ~ 1 in./sec to ~ 1 ft/sec. Contact pressures on pin/plate

7693-20003

Figure 13. Schematic of Short-Stroke Oscillating Wear Test Apparatus

specimens were varied from ~ 300 to ~ 2000 psi. Wire wrap/cladding tests were made with wire-to-cladding cylinder angles at values of 12, 20.5, and 29.5 degrees, with the same stroke length and maximum velocity ranges as for the pin/plate tests. Loads on the three wire wrap/cladding tests were 1.5, 5, and 20 lb, respectively.

B. FRICTION AND WEAR TESTING

As of the beginning of this reporting period in January 1971, three pin/plate tests, involving 20% cold-worked (CW) Type 316 SS rubbing against itself, were in a 1200°F dwell mode after experiencing 500 in. of short-stroke rubbing. These three tests involved a 300-psi contact pressure with stroke length as the parameter varied. During January, the planned 1-month dwell was completed and sliding contact was again initiated. Rubbing was continued at 1200°F until very high apparent coefficients of friction (to the point of seizing) were encountered. The oxygen content during this entire test was in the 7 to 10-ppm range. The friction values encountered during an individual stroke are shown in the lower half of Figure 14. Data for an individual stroke were obtained after every 100 in. of travel.

Subsequent rubbing at lower temperatures indicated that the apparent coefficient of sliding friction was lowered to values near or below unity, even on those specimens where 1200°F rubbing had increased the apparent coefficient of sliding friction to values that had caused seizing ($f > 7$). Lowering of the friction is believed to be due to the formation of an $\text{Na}_x\text{Cr}_y\text{O}_z$ lubricative coating on the stainless-steel surfaces in certain ranges of oxygen content and temperature. Comparisons of free energy values for oxygen dissolved in sodium (calculated) and oxygen in the complex $\text{Na}_x\text{Cr}_y\text{O}_z$ (empirically estimated from friction experiments) indicated that the complex would be definitely unstable at 1200°F, possibly stable at 1160°F, and definitely stable at 880°F with oxygen in the 7 to 10-ppm range. Tests to obtain some information on kinetics were performed at 880°F. The coefficient of friction, at 880°F, decreased with time until, after ~ 16 hr, it reached a value characteristic of the Na-Cr-O complex layer. Friction values at 450°F were ~ 0.7 in these tests.

LMEC-82041

Figure 14. Friction vs Time During Individual Stroke for Pin/Plate Tests

The length of stroke for these three tests was based upon d , the pin diameter (which was 0.24 in.), and were $<d/2$ (0.10 in.), $\sim d/2$ (0.13 in.), and $>d/2$ (0.18 in.). The wear process for stainless steel involves transfer back and forth of the same wear debris, so the total amount of material loss from either surface was not particularly drastic. Nevertheless, the $<d/2$ stroke resulted in less than 1/2 the pin wear experienced by the $d/2$ and $>d/2$ strokes. The value of the "wear coefficient" (k) for these tests was calculated from the original height (h_0) minus the maximum final pin height (h) divided by the total rubbing distance (s), all divided by the apparent contact pressure (p). Thus,

$$[(h_0 - h)/S]/P = K, \text{ in units of (in./in.)/psi}$$

K values on the order of 3×10^{-9} (in./in.)/psi for the $<d/2$ -in. stroke (0.099 in.) and $\sim 6 \times 10^{-8}$ (in./in.)/psi for the $d/2$ and $>d/2$ -in. strokes were measured. These results (see Table 11) are applicable for 300-psi contact pressure applications of 20% CW Type 316 SS undergoing short-stroke oscillating rubbing for 100 to 700 in. of total travel in sodium at 1200°F with oxygen contents too low for formation of a lubricative $\text{Na}_x\text{Cr}_y\text{O}_z$ layer.

TABLE 11

FRICTION AND WEAR DATA
20% CW TYPE 316 SS PINS RUBBING AGAINST 20% CW TYPE 316 SS PLATE

Sodium Conditions	Contact Pressure (psi)	Stroke Length		Wear Coefficient [(in./in.)/psi]	Friction at 1200°F
		in.	pin diam/ x		
1200°F, ~ 8 ppm	300	0.10	$d/2.4$ ($<d/2$)	3×10^{-9}	Galled
1200°F, ~ 8 ppm	300	0.13	$d/1.9$ ($\sim d/2$)	6×10^{-9}	Galled
1200°F, ~ 8 ppm	300	0.18	$d/1.3$ ($>d/2$)	6×10^{-9}	Galled

The wear coefficient does not give the total amount of material on a pin adversely affected by wear. "Hills" and "valleys" are present after rubbing (see Figure 15) and, since the wear coefficient involves measurement only to the top of the highest hill, the distance from highest hill to deepest valley should be added. Inclusion of this additional amount of wear raised the wear coefficients from the 3×10^{-9} to 6×10^{-9} range to $\sim 1 \times 10^{-8}$ (in./in.)/psi.

Figure 15. Section Through 20% CW Type 316 SS Pin and 20% CW Type 316 SS Plate after Rubbing 700 in. ($< d/2$, 0.099 in. stroke) at 1200°F (and a few inches more at various temperatures down to 450°F. Both surfaces were originally flat.)

The photomicrographs in Figure 16 indicate that an additional 0.5×10^{-8} (in./in.)/psi should be added to account for the thickness of the 20% CW Type 316 SS transferred back and forth, ending up on the pin. The thickness appears to be the same, $\sim 1\text{-}1/4$ mils, under both hill and valley; so the best value for an overall wear plus localized wear plus damaged layer appears to be 1.5×10^{-8} (in./in.)/psi for 20% CW Type 316 SS rubbing against itself. In the case of the Inconel-718/20% CW Type 316 SS at 2000 psi, the Inconel 718 pins gained in length at the expense of the 20% CW Type 316 SS plate material.

The etched post-test 20% CW Type 316 SS shows carbide precipitated along slip planes to the extent that the observation can be made that, metallurgically, the post-test material is an entirely different class of material than the pretest alloy. After test, the alloy is made up of subgrain volumes of ductile metal, approximately 10 microns on a side, enclosed in brittle planes of carbide. Before test, the alloy is composed of cold-worked (but ductile) grains, more than 100 microns across, with no continuous carbide boundaries. The increased frictional forces encountered with this material may possibly relate to these microstructural changes.

Figure 17 shows a scar mark and pin surface as seen by scanning electron microscopy using a montage technique. The shape of a debris particle is of considerable interest, in particular whether it is spherical or plow-shaped. Luckily, a few debris particles had escaped being smeared and Figure 17 shows that their shapes were clearly plow-shaped, rather than spherical. Thus, these debris particles plowed rather than rolled. This information is important in understanding the friction-and-wear mechanisms for this particular material combination.

Figure 18 clearly indicates that the pin and plate surfaces were quite roughened in these tests.

Figures 19 and 20 are the best estimates, as of June 30, 1971, of the various friction regimes with which the designer of sodium systems must contend. The level of oxygen in the sodium affects the upper temperature limit of the lubricative sodium - chromium-oxygen complex ($\text{Na}_x\text{Cr}_y\text{O}_z$). The percent, and hence activity, of chromium in the metal surface layer will affect both upper and lower temperature limits of the $\text{Na}_x\text{Cr}_y\text{O}_z$ layer. Higher chromium content

LMEC-P82043

Figure 16. Photomicrographs (100X) of Etched Sections of Pin and Plate Shown in Figure 15. (Note layer of highly disturbed metal, approximately 1-1/4 mil in thickness, on each rubbing surface.)

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LMEC-P82044

Figure 17. Scanning Electron Microscope Photo Montage of 20% CW Type 316 SS Plate Scar Mark and Pin Shown in Figure 15. (Plate scar at 3/4 of pin mag.) (Note "cold butter"-type smearing of surfaces and plow shape of unsmeared wear debris particles at very end of scar mark.)

LMEC-71-7

LMEC-P82045

Figure 18. Photomicrographs of 0.24-in.-diameter Pins of 20% CW Type 316 SS
Corresponding Scar Marks on 20% CW Type 316 SS Plate after ~ 700 in.
of Sliding Contact

LMEC-71-7

Figure 19. Friction Regimes and Thermodynamic Relationships for Stainless Steel/Sodium/Oxygen Systems

LMEC-82047

Figure 20. Estimation of Friction Regimes as a Function of Temperature and Oxygen Content in Liquid Sodium

will most probably lower the $1/z \text{Na}_x\text{Cr}_y\text{O}_z$ band shown in Figure 19, thus extending the stable range of this complex at both ends of the temperature scale. Unfortunately, depleting the surface in chromium will most probably decrease the stability range of this lubricant. Interpretation of friction data on alloys containing the element chromium should be made with the aid of these, or similar, updated charts.

Summarizing these 20% CW Type 316 SS pin/20% CW Type 316 SS plate test results, while the wear was not excessive, the apparent friction values encountered at 1200°F ultimately became so high that this material combination appears to be unacceptable for the wear-pad applications as presently designed.

The next series of pin/plate tests involved Inconel 718* pins rubbing against 20% CW Type 316 SS plates. An oxygen limit of 5 ppm was imposed on this and future tests as contrasted to the previous limit of 10 ppm. One of the parameters varied in this series of tests was the contact pressure. These were ~2000, ~600, and ~300 psi. The pins under ~2000-psi pressure had a diameter of 0.102 in. and a stroke length of ~0.1 in. The pins under ~600 psi had a diameter of 0.142 and a stroke length of 0.03 in. The pins under ~300 psi had a diameter 0.242 and a stroke length of 0.03 in. This is summarized in Table 12, along with the friction and wear behavior.

Data on the 200 psi Inconel 718/20% CW Type 316 SS tests obtained during subsequent runs made at 1000, 900, 800, 600, and 450°F are currently being reduced. The highest dynamic friction values observed in the 1000° to 600°F range was unity; but at 450°F, friction values were between 1 and 2. The friction data from the 1160°F run have been reduced and are presented herein (Figure 14) from oscillatory-motion friction tests of Inconel-718 pins rubbing against 20% CW Type 316 SS plates in 0.10-in. strokes at 2000-psi contact pressure while immersed in 1160°F sodium with <5 ppm oxygen. Previous friction data from 20% CW Type 316 SS pins/20% CW plates run at 1200°F are given for comparison. Raw data from Inconel-718/20% CW Type 316 SS plates at lower contact pressures have not, as yet, been reduced. Photographic data

*Thermomechanical treatment as follows: Rough machined, heated in argon to 1950°F, slow-cooled at 50°/hr to 1400°F, held 1 hr, again cooled slowly to 1200°F, held 10 hr, air-cooled, and final machined.

TABLE 12

DATA FROM TESTS OF INCONEL-718 PINS RUBBING AGAINST
20% CW TYPE 316 SS PLATE

Sodium Conditions	Contact Pressure (psi)	Stroke Length		Wear Behavior	Friction at 1160 °F
		in.	pin diam/x		
1160 °F, <3 ppm O	~2000	~0.1	~d/1	Pins carved into plate	1.5
1160 °F, <3 ppm O	~ 600	0.03	~d/4.7	Slight build- up on pins	0.43
1160 °F, <3 ppm O	~ 300	0.03	~d/8	*	*

*To be analyzed next report period.

from the 20% CW Type 316 SS pins/20% CW plates are also analyzed in this report (Figures 15 through 18).

Each individual curve in Figure 14 represents the friction value encountered during a single stroke. Each stroke took 2.7 sec and extended for ~0.1 in. All the strokes recorded in this figure were for movement in the same direction. Readings were taken after approximately every 100 in. of total travel (i. e., each reading after slightly over 1000 individual strokes). The small circles at the top of each strip of curves represent the stroke and pin diameter relationships (magnified, but to scale).

After approximately 500 in. of sliding contact was achieved in the 1200 °F, 20% CW Type 316 SS/20% CW Type 316 SS tests (300-psi contact pressure), the apparent coefficient of friction values began to indicate that an increasingly greater percent of surface area was dynamically "self-welding." The 1160 °F Inconel-718/20% CW Type 316 SS specimens (2000-psi contact pressure) did not experience increases in the extent of dynamic self-welding throughout the imposed breakaway coefficient on this couple after the 1-week dwell at 1160 °F under 2000-psi contact pressure.

Rubbing in the set of three tests involving 20% CW Type 316 wire wrap/20% CW cladding was started during the last few days of this report period

Initial rubbing was in sodium at 1160°F. Oxygen content was in the 3 to 5-ppm range* at temperature. Initial friction measurements showed "apparent" friction values between 5 and 10 for the 1.5-lb load, half that high for the 5-lb load, and between 1 and 2 for the 20-lb load. Accurate friction values will be reported after these data have been reduced. These initial readings indicate that the apparent coefficient of friction builds up until enough shear stress is built up to machine away metal. The Hertzian stresses are very much higher than the yield strength of these materials even with the 1.5-lb load. Thus, during the first few strokes, actual machining is occurring, rather than rubbing. This continues until a suitably sized flat contact area between wire wrap and clad is established.

During the next reporting period, rubbing in 1160°F sodium on the three wire wrap/clad tests will continue for 1000 in. of total travel. Friction data will be obtained at temperatures up to 1350°F. Pin/plate tests will be performed on other candidate materials under conditions prototypical of various in-core configurations.

*All oxygen measurements reported here utilize the total consumption method. Extrusion method gave oxygen results 1/3 to 1/2 the values reported here.

III. RESULTS OF HALLAM IHX EXAMINATION

K. L. Adler

Idaho Nuclear Corporation (INC) has performed a destructive examination of one of the six intermediate heat exchangers (IHX) from the Hallam Nuclear Power Facility (HNPF). The primary objective of the examination was the accumulation of data for assessing crevice corrosion problems and evaluating operational deterioration associated with front-face-type, tube-to-tubesheet welded joints in sodium heat transfer equipment.

Metallurgical evaluation of the front-face tube-to-tubesheet welds found the welds to be structurally sound. The examination revealed stress corrosion cracking in some tubes and ligaments of the top tubesheet but not in the bottom tubesheet.

The final examination report by INC* stated the following conclusions:

- "1. The front face tube-to-tubesheet welds were found to be structurally sound.
- "2. The tube rolling was not effective in transferring the tube loading to the tubesheet in most samples examined. Therefore the welds functioned as a load bearing mechanism as well as a seal. The presence of sodium in the tube-to-tubesheet crevice did not damage the tubes or the tubesheet since no correlation was found between sodium deposits in the crevices and tube or tubesheet ligament cracks.
- "3. The cracking found in the tubes and the ligaments of the top tubesheet were caused by stress corrosion. This conclusion is based on the following facts: (a) transgranular cracks were found in several tubes and ligaments of the top tubesheet, (b) the cracks found in the tubes were adjacent to the cracks in the tubesheet ligaments (with one exception), (c) chlorides were found in the tube-to-tubesheet crevices, and (d) the top tubesheet was at the hot, inlet end of the heat exchanger and thus experienced added thermal stresses.

"We conclude that the bottom or outlet tubesheet did not fail or crack similar to the top tubesheet due to different service conditions.

- "4. No other evidence of operational damage was found."

*W. L. Random, "Destructive Examination of a Hallam Nuclear Power Facility Intermediate Heat Exchanger," IN-1477, Idaho Nuclear Corporation (April 1971)

IV. PUMP SEAL TEST PROGRAM
(LMFBR Program Plan 3.11.3.1)
D. J. Kniley

A. INTRODUCTION

A pump shaft seal of the design presently in service at the Enrico Fermi Atomic Power Plant (EFAPP) was operated in the newly constructed Pump Seal Test Facility (PSTF). It was the purpose of the overall program to use the seal as the basis for qualifying the PSTF for testing shaft seals. Detailed program results are reported in LMEC TDR 71-3. Highlights of seal performance data and seal oil/sodium vapor interaction are presented herein.

B. DESCRIPTION OF THE PUMP SEAL TEST ASSEMBLY

The test stand (Figures 21 through 23) is a vertical structure consisting of a drive assembly mounted on a test seal adapter which, in turn, is mounted on the sodium test vessel supported in the stand framework. Support adjustment and seal shaft alignment are accomplished by means of a spindle designed with total radial axial and angular (vertical) alignment flexibility. The shaft is driven through flexible couplings and a torque transducer by a variable-speed, 50-hp dc electric motor powered by a motor generator (MG) set.

The seal used to qualify the test facility is an existing design now operating in the EFAPP (Figure 24). It is a mechanical face-type seal with opposed, spring-loaded, overbalanced rotating seal faces with Buna N U-cups as secondary seals. The mating ring, which is in contact with the rotating seal face, is stationary, utilizing O-rings as the combination antirotation device and secondary seal. The seal utilizes flow oil as a lubricant and coolant. The entire rotating assembly is mounted to a shaft sleeve keyed to rotate with the shaft. This sleeve, in the pump, covers a shaft coupling. On the PSTF unit the sleeve serves to mockup the clearances between the sleeve and shaft, the shaft not being broken at that level. The PSTF seal duplicates all functional characteristics of the Fermi pump seal. Peripheral hardware was reduced in diameter so that it could be accommodated within the PSTF support housing.

The test seal package (Figures 25 and 26) is a self-contained unit capable of supporting all seal needs, including a complete lubrication system, a part of

Figure 21. Pump Seal Test Facility Schematic

Figure 22. Comparison of PSTF to a Typical Pump

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7693-40501CN

Figure 23. Pump Seal Test Facility

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LMEC-34031

Figure 24. Pump Shaft Seal Assembly

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PARTS LIST		
ITEM NO.	QUANTITY	PART NAME
1	1	Motor - Blower
2	1	Bracket - Blower
3	1	O-ring
4	1	O-ring
5	1	Reservoir - Oil
6	3	Cap Screw
7	7	Cover - Seal
8	8	Cap Screw
9	1	O-ring
10	1	O-ring
11	6	Cap Screw - Soc. Head
12	1	Key
13	1	Nut - Sleeve
14	1	O-ring
15	1	Holder - Thermocouple
16	1	Bushing - Mounting
17	2	Thermocouple
18	2	Fitting
19	2	O-ring
20	1	Gage - View
21	1	Hose - Rubber - Air
22	1	Pump - Motor Driven Oil
23	1	Blower
24	1	Flange - Routing
25	8	Cap Screw
26	1	Adapter
27	1	Glass - Sight
28	6	Cap Screw
29	1	Frame - Sight Glass
30	8	Cap Screw
31	1	O-ring
32	1	Housing - Seal
33	4	Nut - Slotted Hex
34	4	Stud
35	36	Pipe - Spacer
36	1	Shaft
37	2	Detector - Proximitior
38	2	Nut
39	1	O-ring
40	1	O-ring
41	9	Plate - Heat Barrier
42	2	O-ring
43	2	Nut
44	2	Holder - Proximitior
45	1	Valve - Drain
46	2	Valve - Control

SEAL PARTS		
ITEM NO.	QUANTITY	PART NAME
S-1	1	Sleeve - Shaft
S-3	1	Pin - Drive
S-4	2	U-Cup
S-13	2	O-ring
S-14	2	Face - Stationary
S-15	2	Face - Rotating
S-16	36	Spring - Coil
S-17	1	Holder - Spring
S-19	1	O-ring
S-25	1	Key
S-43	1	Sleeve - Spring Holder
S-60	1	Follower - U-Cup
S-85	36	Pin

Figure 25
Test Assembly
8-in. Shaft Seal
Primary Sodium Pump

Byron Jackson Pump Div.
Borg-Warner Corp.
Drawing No. 1F-6724

LMEC-71-7

7693-40444CN

Figure 26. Test Seal Installation

which is an oil-to-air heat exchanger. Air for the heat exchanger is provided by a centrifugal blower. The lubrication-cooling system is designed to operate at full flow and full pressure during all seal operations. Pressure in the seal cavity is controlled by orifice plugs located in the outlet ports of the heat exchanger chamber, providing (at design flow) full pressure to the heat exchanger and seal cavity. The oil pump, a positive displacement gear pump, maintains essentially full flow below the rated output pressure of the unit. The oil filter (used by the LMEC) is a 5-micron, sintered stainless steel unit. Filter contamination is monitored by the pressure drop across the filter. A substantial pressure increase can be tolerated before flow drops to the point where seal cooling is threatened. The oil system also contains a flow transducer, a seal cavity pressure transducer, and a temperature transducer located adjacent to the upper rotating face just before the oil leaving the seal cavity to be cooled in the heat exchanger chamber.

Oil leakage is directed away from the seal housing by lines draining from three leakage chambers. Leakage from the upper seal must first pool and fill up an annulus until a port is reached opening directly to the upper leakage tube sloping down into a catch beaker. The lower leakage drips down away from the seal into a catch trough which is drained by a leakage line into a sealed beaker. This chamber is part of the cover gas system and as such must remain sealed during normal operation. When the leakage is drained from the beaker, an in-line valve seals the line. A secondary catch reservoir was provided for the PSTF seal so that the leakage region could be seen through a viewing window in the outer seal housing wall. This enlarged chamber, approximately 10-gal capacity, provides a backup chamber for critical seal leakage into the cover gas side of the system. It is in this region where oil and oil vapors are exposed to sodium vapors which have been carried up into the colder regions by diffusion and thermal convective flow. Total lower seal leakage must be additive from both lower lines.

The seal oil used at Fermi is a fluorocarbon base oil, trade named Fluorolube (supplied by Hooker Chemical Co.). It is a nonhydrogenous oil used by Fermi to satisfy the lubricant specification requiring an oil with no hydrogen compounds. Included in the test results is a section on the experience at LMEC with Fluorolube, a material which by nature reacts violently with sodium.

C. OPERATION OF THE SEAL

Test series I through VIII (Tables 13 and 14) were conducted to systematically identify the operating characteristics of the total PSTF.

Figure 27 presents a temperature profile of the PSTF using the Fermi seal without seal operating effects. The upper curve was plotted from data obtained during test series VI when sodium temperatures reached a PSTF design maximum of 1200°F (beyond Fermi system design conditions). Numbers at the points clarify ordinate values. Temperature values at each location varied directly with the temperature of the sodium, except that spindle bearing temperatures were apparently unaffected by sodium temperature. Seal oil temperature remained below 85°F during all elevated temperature runs, with the gas temperature in the shaft annulus 6 in. below the inboard seal remaining below 145°F.

During PSTF seal operation at speeds to 860 rpm, the seal oil temperature remained below 163°F (Figure 28), with short-term peaks (not plotted) of up to 167°F. The time scale on the plot is from the start of the run. Full speed was attained within 60 sec from start of drive motor (start of run). Numbers at the points clarify ordinate values. In general, the higher shaft speeds resulted in higher seal oil temperatures as well as an increased rate of temperature rise. Temperature stabilization was achieved usually within 3 to 4 hr. Stabilization temperatures ranged from 95°F during the 70-rpm run to 163°F during the 1100-rpm run. Runs at shaft speeds of 1187 and 1200 rpm were not plotted because the water cooling coils on the oil filter were in operation.

The LMEC PSTF seal was operating with the highest base face loading (22 psi) used at Fermi, which would result in the highest power consumption represented with highest heat removal requirement.

A sample of seal torque data is reported in Figure 29. Although the initial maximum shaft torque for the runs at 200, 860, 1000, and 1200 rpm varied inversely with shaft speed, the value may be dependent also upon: (1) the condition of the seal faces resulting from prior testing, (2) the temperature (i. e., viscosity) of the oil, and (3) the ability of the seal faces to form a lubricating film of oil between their surfaces. The curves for 70 rpm and 600 rpm do not follow the tendency for higher initial torque at lower speeds. For all speeds

TABLE 13
SEAL TEST FACILITY QUALIFICATION REQUIREMENTS

Operating Conditions	Test Series											
	I			II			III			IV		
	Test No.			Test No.			Test No.			Test No.		
	1	2	3	1	2	3	1	2	3	1	2	3
Shaft speed (rpm)	70	70	70	300	600	860	300	600	860	300	600	860
Sodium temperature (°F)	450	750	1000	450	450	450	750	750	750	1000	1000	1000
Cover gas pressure (psig)	1	1	1	1	1	1	1	1	1	1	1	1
Seal oil pressure (psig)	3 to 4	3 to 4	3 to 4	3 to 4	3 to 4	3 to 4	3 to 4	3 to 4	3 to 4	3 to 4	3 to 4	3 to 4
Seal oil temperature (°F)												
Normal	110 to 155	110 to 155	110 to 155	110 to 155	110 to 155	110 to 155	110 to 155	110 to 155	110 to 155	110 to 155	110 to 155	110 to 155
Maximum	165	165	165	165	165	165	165	165	165	165	165	165
Test duration	Tested until all basic parameters have been stabilized for 4 hr or more.											

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TABLE 14
FACILITY OPERATION BEYOND FERMI SEAL DESIGN CONDITIONS

Operating Conditions	Test Series			
	V	VI	VII	VIII
Shaft speed (rpm)	1000*	1100	1200	1187*
Sodium temperature (°F)	1050*	1200	1200	1050*
Cover gas pressure (psig)	1	1	1	1
Seal oil pressure (psig)	3 to 4	3 to 4	3 to 4	3 to 4
Seal oil temperature (°F)				
Normal	110 to 155	110 to 155	110 to 155	110 to 155
Maximum	165	165	165	165
Test duration (hr)	240	240	240	2160

*FFTF proposed shaft speed and sodium temperature

LMEC-34032

Figure 27. Thermal Profile of Test Assembly - Shaft Assembly

LMEC-34033

Figure 28. Seal Oil Temperature vs Time

LMEC-34034

Figure 29. Spindle Shaft - Torque vs Time

considered, steady-state shaft torque averaged approximately 57% of the initial maximum value, the average steady-state value being 300 in.-lb.

After 1 hr of operation, the steady-state shaft torque over all speeds ranged from 270 in.-lb to 362 in.-lb (Figure 29). After 8 hr, the torque range had narrowed to values within 280 in.-lb to 312 in.-lb. Considering all speeds, the average steady-state shaft torque was 300 in.-lb. From this data it appears that the seal faces have a greater ability to form a lubricating film of oil between their surfaces as shaft speed increases.

Table 15 provides a sampling of periods of torque spiking or peaking which, with startup periods, account for a significant amount of face wear. Early tests included many spikes of magnitudes exceeding 450 in.-lb, whereas the later runs of 1000-rpm shaft speed, and higher, had noticeably fewer spikes above 450-in.-lb. Prior to July 15, no torque spike could be correlated definitely with a decrease in shaft speed. The torque trace during the final test (1187 rpm over 1050°F sodium) is unusually devoid of spikes.

Table 16 presents a sampling of PSTF seal leakage data. During periods of continuous running, leakage rates past the inboard seal varied from 0.1 ml/hr near the start of testing to 24.6 ml/hr near the end of testing. Outboard seal leakage rates during continuous running varied from 0.0 ml/hr near the start of testing to approximately 54.0 ml/hr near the end of testing. Although the inboard seal leakage rate increased noticeably after the misalignment tests, the outboard seal did not immediately suffer an increase in leakage. Zero measurable leakage was typical for the outboard seal during the period of continuous running. Prior to the misalignment tests, inboard seal leakage rates varied directly with accumulated test time and shaft speed. The separate influence of either of those factors cannot be discerned from the existing data. Within the recorded range of oil temperatures, leakage rate apparently was not affected by minor changes in oil viscosity. Leakage rates during the static 50-psig pressure test were 7.0 ml/hr for the inboard seal and 25.6 ml/hr for the outboard seal.

Seal leakage rates (Table 16) obtained for PSTF seal operation compared reasonably well with available EFAPP seal leakage values (Table 17). PSTF seal leakage rates are listed so that the effects of startups as well as operating

TABLE 15
HIGHLIGHTS OF FERMI SEAL QUALIFIED* SPIKES
IN INDICATED SHAFT TORQUE
(Not Difference Between Maximum and Steady-State Values)

Test	Date	Spike Value Max. (in.-lb)	Duration of Spike (min)	Test	Date	Spike Value Max. (in.-lb)	Duration of Spike (min)
	7-15-70	410 to 512	Continuous	V-1	10-7-70	410	<1
	7-22-70	400	5			418	3
	↓	400	5		↓	410	1
	↓	400	5		↓	402	<1
	↓	500	23		↓	433	2
	7-29-70	413	1		↓	410	1
	7-29-70	410	<1		↓	450	1
	↓	440	10		↓	410	2
	↓	460	15		↓	410	<1
	↓	451	4		10-15-70	412	<1
II-3	9-8-70	368	2		↓	425	3
II-3	↓	380	2		↓	423	<1
II-3	↓	390	3		↓	395	<1
IV-2	9-23-70	392	2	VI-1	10-23-70	400	2
IV-2	↓	340	1	↓	10-30-70	405	<1
IV-2	↓	321	<1	↓	10-30-70	375	1
IV-2	↓	321	1	VII-1	10-30-70	403	2
IV-2	↓	334	2	↓	11-11-70	375	<1
IV-2	↓	321	<1	↓	↓	356	<1
IV-2	↓	340	Intermit.	↓	↓	345	1
IV-3	9-30-70	420	2	VIII-1	12-2-70	403	<1
IV-3	9-30-70	340	Intermit.	VIII-1	12-2-70	348	<1
						395	3

*Corresponding decrease in shaft speed.

TABLE 16
HIGHLIGHTS OF FERMI TYPE SEAL LEAKAGE
(Sheet 1 of 2)

Time (hr)	Date of Measure- ment	Hours at Recorded Speed	Shaft Speed (rpm)	TR2-5 Sodium Temper- ature (°F)	TR4-4 Annulus Temper- ature (°F)	TR4-2 Oil Temper- ature (°F)	Inboard Seal		Outboard Seal	
							Leakage (ml)	Rate (ml/hr)	Leakage (ml)	Rate (ml/hr)
1630	7-28-70	2 starts 7.7	860	No sodium	104	150	10	1.3	5	0.7
1615	8-7-70	1 start 5.1	70	No sodium	80	96	0	0.0	0	0.0
1133	8-12-70	1 start 2.1	860	No sodium	67 to 86	90 to 145.5	1	0.5	15	7.2
1525	8-26-70	1 start 6.0	70	450	97 to 102	83 to 94	0	0.0	4	0.7
1000	8-31-70	3 starts 8.5	300	750	125	95	6	0.7	73	8.6
0830	9-8-70	88.2	0	450	98	77	7	0.1	1	0.0
1625	9-8-70	1 start 7.0	860	450	132	160	15	2.1	186	26.0
0845	9-14-70	64.4	860 to 800	750	133	133	120	1.9	0	0.0
0800	9-23-70	24.0	0	1000	136	80	0	0.0	0	0.0
0800	9-30-70	24.0	860 to 830	1000	165	151	44	1.8	3	0.1
0800	10-7-70	2 starts 23.7	1000	1050	159	150	53	2.2	1	0.0
0800	10-15-70	24.0	1000	1050	160	155	51	2.1	4	0.2
0800	10-23-70	24.0	1100	1200	175	157	101	4.2	2	0.1
0800	10-30-70	24.0	0	1200	156	95	7	0.3	0	0.0

TABLE 16
HIGHLIGHTS OF FERMI TYPE SEAL LEAKAGE
(Sheet 2 of 2)

Time (hr)	Date of Measure- ment	Hours at Recorded Speed	Shaft Speed (rpm)	TR2-5 Sodium Temper- ature (°F)	TR4-4 Annulus Temper- ature (°F)	TR4-2 Oil Temper- ature (°F)	Inboard Seal		Outboard Seal	
							Leakage (ml)	Rate (ml/hr)	Leakage (ml)	Rate (ml/hr)
1600	10-30-70	1 start	1100	1200	172	158	0	0.0	131	46.8
0800	11-11-70	24.0	1200	1200	173	149	137	5.7	0	0.0
0800	11-19-70	24.0	0	1200	~145	~84	49	2.0	41	1.7
1600	12-2-70	1 start 7.0	1187	1050	158	151	71	10.1	239	34.2
0800*	12-7-70	1 start 18.2	1187	1050	159	157	817	44.9	0	0.0
1610*	12-7-70	8.2	1187	1050	160	156	120	14.6	0	0.0
0800*	12-8-70	15.8	0	1050	139	83	17	1.1	0	0.0
1604†	12-8-70	1 start 2.4	1187	1050	165	158	61	25.4	46	19.2
0800†	12-9-70	15.9	1187	1050	165	156	485	30.5	10	0.6
0800	12-15-70	1 start 24.0	1187	1050	160	155	140	5.8	0	0.0
0800	12-22-70	16.0	1187	1050	158	156	394	24.6	16	1.0
0800	12-29-70	16.0	1187	1050	157	154	340	21.2	0	0.0
1600	12-29-70	8.0	1187	1050	163	160	155	19.4	0	0.0
0800	1-6-71	23.0	1187	1050	160	157	410	1.8	47	2.0
0800	1-8-71	16.0	1187	1050	153	152	292	18.3	100	6.3
1600	1-8-71	8.0	1187	1050	160	156	180	22.5	22	2.8
0800	1-12-71	16.0	1187	1050	156	153	227	14.2	22	1.4
035	1-12-71	2.0	1187	1050	156	149	43	21.5	6	3.0

*0.1' Misalignment Test †0.2' Misalignment Test

TABLE 17
SERVICE TO EFAPP PUMP SEALS
(Sheet 1 of 2)

Date	Pump No. 1		Pump No. 2		Pump No. 3	
	Repair	Oil Added (cc)	Repair	Oil Added (cc)	Repair	Oil Added (cc)
6-6-61	-	1,900	-	-	-	1,420
7-7-61	-	-	-	-	x	-
12-21-61	x	-	-	-	-	-
3-19-62	-	-	-	-	x	-
12 - 62	-	-	x	-	-	-
4-26-63	-	3,800	-	-	-	-
4 - 63	-	-	-	-	x	-
5-22-63	-	-	-	-	-	3,780
8-30-63	-	-	x	(changed)	-	-
10-16-63	-	1,900	-	1,900	-	1,900
11-19-63	-	-	-	-	-	3,780
12-6-63	-	-	-	-	-	3,780
1 - 64	x	(changed)	-	-	-	-
2-28-64	-	-	-	-	x	(changed)
3-19-64	-	-	-	3,780	-	-
5-19-64	x	-	-	-	-	-
5-25-64	-	-	-	-	-	2,370
5-27-64	-	-	x	(changed)	-	-
7-23-64	-	1,420	-	-	-	1,900
8-12-64	-	1,420	-	-	-	-
9-29-64	-	-	-	-	-	3,780
10-2-64	-	-	-	3,780	-	-
10-5-64	-	2,180	-	-	-	-
1-11-65	-	-	-	-	x	-
1-14-65	-	-	-	2,070	-	-
1-28-65	-	-	-	-	-	3,780
2-25-65	-	-	-	-	-	11,350
3-8-65	-	-	-	-	x	12,500
3-9-65	-	1,810	-	-	-	-

TABLE 17
SERVICE TO EFAPP PUMP SEALS
(Sheet 2 of 2)

Date	Pump No. 1		Pump No. 2		Pump No. 3	
	Repair	Oil Added (cc)	Repair	Oil Added (cc)	Repair	Oil Added (cc)
4-5-65	-	-	-	-	-	2,840
5-19-65	-	-	-	-	-	594
6-1-65	-	2,130	-	-	-	-
7-12-65	-	-	-	-	x	-
7-15-65	-	-	-	-	-	3,780
10-1-65	-	950	-	-	-	1,900
11-4-65	-	-	-	-	-	1,660
12-6-65	-	-	-	-	-	1,420
12-12-65	-	-	-	-	-	7,570
12-21-65	-	1,420	-	1,420	-	-
2-10-66	-	2,840	-	-	-	-
3-21-66	-	-	-	-	-	1,900
3-29-66	-	-	-	-	x	-
5-10-66	x	-	-	-	-	-
Totals	4 times	21,770	3 times	12,950	8 times	70,584
Total operating time (hr)	23,085		20,880		20,012	
Overall leakage rate (cc/hr)	0.95		0.62		3.53	

oil temperatures can be evaluated. Evaluating seal leakage with time does not provide a meaningful picture as listed in Table 16 because of the numerous effects of high leakage startup periods. Extracting periods of seal operation without interruption does indicate a service life trend (Table 18).

Available Fermi seal information provides the date of oil replenishment and the amount of oil required to refill the lubrication reservoir. Total seal oil consumption (leakage) over the operating history of the seals provides the leakage rates given in Table 17. These values are lower than the overall trend established by the PSTF seal (Table 16). If the periods of continuous seal operation are considered for long-term comparison (Table 18), leakage rates compare much more favorably. In general, periods of high seal leakage occur during startup where seal torque is also highest.

Table 18 presents the daily leakage values both as absolute leakage and as leakage rate. Startup leakage varied quite severely in certain cases, leaking as much as 225 ml in the first half hour of operation. This behavior was erratic and, in general, leakages could not be favorably displayed as a trend curve. When the shaft was misaligned to demonstrate the ability of the PSTF to operate with a nominal $0^{\circ}1'$, $0^{\circ}2'$ shaft misalignment from the vertical, seal leakage increased very noticeably. These values are noted in Table 18. Following these runs, the shaft was realigned and operation was resumed. However, leakage was unpredictable, maintaining higher-than-normal rates. Consequently, it was decided to shut down the seal for inspection and refurbishment.

Test series V, VI, and VII involved test conditions beyond those experienced at Fermi. Shaft speed was increased to 1000, 1100, then 1200 rpm, the PSTF operating maximum. Sodium temperatures were raised to 1050°F , the proposed FFTF seal test condition; then to 1200°F , the PSTF operating maximum. During these higher temperature runs the oil temperature ran much closer to the 165°F maximum experienced at Fermi. Changes in room ambient quite noticeably affected the oil temperature. A room air conditioner was placed at the inlet to the blower to regulate the inlet temperature to the heat exchanger system. Cooling coils were added to the oil filter as an emergency cooling source because of difficulties experienced with operation

TABLE 18
SUMMARY OF CONTINUOUS OPERATION SEAL LEAKAGE
(Sheet 1 of 2)

Date	Shaft Speed (rpm)	Annulus Temper- ature (°F)	Oil Temper- ature (°F)	Leakage		Sodium Temper- ature (°F)
				Inboard Seal (cc/hr)	Outboard Seal (cc/hr)	
7-29-70	860	104	150	1.5	0.3	RT
7-30-70	860	107	155	1.8	0.4	RT
9-11-70	860	160	166	4.0	4.6	750
9-14-70	860	133	133	1.9	0.0	750
9-25-70	600	158	135	0.4	0.3	1000
9-29-70	860	164	154	0.1	1.1	1000
9-30-70	860	165	151	1.8	0.1	1000
10-1-70	860	162	153	0.8	0.1	1000
10-2-70	860	157	139	1.5	0.1	1000
10-8-70	1000	160	151	2.5	0.0	1050
10-9-70	1000	161	153	1.3	0.2	1050
10-12-70	1000	162	154	1.4	0.8	1050
10-13-70	1000	161	153	1.2	0.8	1050
10-14-70	1000	162	156	2.6	0.4	1050
10-15-70	1000	160	155	2.1	0.2	1050
10-21-70	1100	175	154	2.2	0.0	1200
10-23-70	1100	175	157	4.2	0.1	1200
10-26-70	1100	171	150	2.3	0.0	1200
10-28-70	1100	176	153	2.0	0.0	1200
10-29-70	1100	176	157	2.9	0.0	1200
11-2-70	1100	168	140	4.6	0.1	1200
11-3-70	1100	168	141	4.1	0.1	1200
11-4-70	1200	175	154	5.4	0.0	1200
11-5-70	1200	174	154	5.8	0.0	1200
11-6-70	1200	177	159	6.9	0.0	1200
11-9-70	1200	175	154	3.4	0.0	1200
11-10-70	1200	171	152	4.4	0.2	1200
11-11-70	1200	173	149	5.7	0.0	1200

TABLE 18
SUMMARY OF CONTINUOUS OPERATION SEAL LEAKAGE
(Sheet 2 of 2)

Date	Shaft Speed (rpm)	Annulus Temper- ature (°F)	Oil Temper- ature (°F)	Leakage		Sodium Temper- ature (°F)
				Inboard Seal (cc/hr)	Outboard Seal (cc/hr)	
11-12-70	1200	175	149	7.6	0.0	1200
11-13-70	1200	173	153	5.7	0.0	1200
11-16-70	1200	178	153	5.2	0.0	1200
12-3-70	1187	153	153	4.4	0.1	1050
12-7-70	1187	0°1' Misalignment Test				
		160	156	14.6	0.0	1050
12-9-70	1187	0°2' Misalignment Test				
		165	156	30.5	0.6	1050
12-11-70	1187	167	161	10.2	0.0	1050
12-16-70	1187	163	156	6.4	0.0	1050
12-17-70	1187	162	154	5.5	0.0	1050
12-18-70	1187	165	153	6.9	0.0	1050
12-22-70	1187	158	156	24.6	1.0	1050
12-22-70	1187	162	160	21.8	1.2	1050
12-29-70	1187	157	154	21.2	0.0	1050
12-29-70	1187	157	153	19.4	0.0	1050
12-30-70	1187	163	160	19.4	0.0	1050
12-30-70	1187	162	162	17.1	0.0	1050
1-5-71	1187	150	146	9.9	0.3	1050
1-6-71	1187	160	157	1.8	2.0	1050
1-8-71	1187	153	152	18.3	6.3	1050
1-8-71	1187	160	156	22.5	2.8	1050
1-11-71	1187	150	150	15.9	54.0	1050
1-11-71	1187	155	151	15.0	1.8	1050
1-12-71	1187	156	153	14.2	1.4	1050
1-12-71	1187	156	149	21.5	3.0	1050

of the air conditioner. Use of the cooling coils would have introduced an unknown heat removal source which would hamper comparative seal operation evaluation at more severe operating conditions. Data obtained during periods of air conditioner outage were not plotted. Operation of the seal at higher speeds with a regulated 72° to 76°F ambient air supply to the blower was within leakage and temperature limits established as normal during early operation within design conditions.

A sample of overall seal operation is included in Table 19. Notations in the extreme right-hand column of the table, and/or with the dates, refer to the corresponding test series, test number, and times of the start and end of the run.

Operation of a seal in the PSTF when compared to "in-pump" seal operation introduces uncertainty with regard to proximity of the sodium to the seal. Seal oil vapors introduced by inboard seal leakage runoff are present in the shaft annulus below the seal. The degree of interaction between the sodium vapor and oil vapor depends on vapor source temperature and distance and path separating the vapor sources.

Fluorolube oil vapor/sodium vapor interaction products were uncovered at EFAPP during installation of new pump check valves. Samples of a black deposit were taken from the pump shaft. Analysis of this deposit confirmed the contents to be reaction products of sodium and Fluorolube. Sodium and Fluorolube (a halocarbon oil) are highly reactive; when present, they react completely, forming such compounds as free carbon, sodium chloride, sodium fluoride, and sodium carbonate. The concentration of sodium in the cover gas is reflected by the content of sodium constituents in the deposits.

Two samples were obtained by PRDC from regions of the Fermi pump: one from the shaft directly below the inboard seal, the other 8 in. down in the annulus. The farther away from the sodium, the lower the sodium compound content in the deposits. On the Fermi pump shaft there was 34% more sodium fluoride 8 in. below the rotating seal than at the seal, and 16% more sodium chloride.

TABLE 19
HIGHLIGHTS OF OVERALL SEAL PERFORMANCE DATA

Date	Start- Stop Cycles	Run Time (hr)	Speed Range (rpm)	Sodium Temperature Range (°F)*	Torque Range (in.-lb)* Start/Shutdown	Oil Pressure Range (psig)* Start/Shutdown	Oil Temp (°F)* Start/Shutdown	Annulus Temp (°F)* Start/Shutdown	Ambient Temp (°F)* Start/Shutdown
9/1/70	1	6	60/90	985/1005	820/270	4.3./4.5	89/98	143.5/143.5	76/88
9/4/70	4	7	600/615	446/451	1000/320	5.3/4.3	75/131	95.5/120	69/78
9/8/70	1	7	840/870	446/455	530/335	5.5/4.8	81.5/150	98/132	73/84
9/23 to 9/25/70	2	44.6	600/645 580/625	1010/1000	583/308(280)	4.3/4.4	89/132(141)	140/153(159)	79/77
9/28/70	1/2	95.2	819/870	1000/990	396/320(302)	5.0/4.9	142/140(163)	163/156(171)	83/70
10/6 to 10/15/70	1	212.8	954/1026	1050/1050 (1010)	812/320(278)	†	150/156(160)	161/160	65/67(80)
10/21 to 10/25/70	2	92.4	1077/1131	1200/1195	510/320(326/ 290)	†	71/164(168)	148/182	59/71(75)
10/30 to 11/3/70	1/2	75.7	1065/1110	1202/1200	512/280(298)	†/5.7	90/143(139)	156/167	72/72
11/9 to 11/16/70	1	167.1	1155/1224	1189/1195	490/296(324)	5.7/5.5	82/150(164)	149/174(179)	73/70(62)
12/2 to 12/3/70	1	24.5	1146/1203	1060/1045	310/300(344) (280)	5.5/5.5	87/155(162)	138/154(159)	66/75(62)
12/4 to 12/7/70	1	72.6	1155/1215	1050/1050	304/270(266)	5.1/5.4	87/156(163)	87/160	78/78(70)
12/8 to 12/9/70	1	22.8	1170/1230	1050/1052	282/276(296)	5.6/5.3	87/160(165)	87/166	74/79
12/9 to 12/11/70	1	43.0	1140/1233	1055/1050	292/296(300) (274)	5.4/5.4(4.8)	92/163(167)	151/164(170)	79/75(66)
12/14 to 12/18/71	1	93.6	1155/1242	1053/1051	280/286(298) (272)	5.9/5.3	84/151(160)	146/162(166)	72/64(76/ 59)
12/21 to 12/22/71	1	30.0	1131/1227	1050/1045	286/284(274) (380)	5.5/5.3	77/162	127/162	69/76
12/28 to 12/30/71	1	54.0	1170/1230	1045/1048	270/280(328)	5.0/5.1	20/162	137/163	70/77(66)
1/7 to 1/12/71	1	118.5	1128/1242	1045/1050	280/256(362)	5.0/5.0(5.3)	88/150(157)	135/155	75/68(63)

*Figures in parentheses reflect off-trend values.

†Data not available.

The free carbon present at the inboard rotating seal was higher by 65% than at the lower level of the annulus; sodium carbonate appeared at the lower level where none was present at the rotating seal. There was no unreacted oil at the lower level, whereas 70% of the sample at the seal was unreacted oil. Contamination samples were obtained from the PSTF during operation in the leakage oil and cover gas, and following the test from deposits found on the shaft. Filtrate from the leakage oil at the rotating seal was also analyzed for sodium reaction products. The results of this analysis and of LMEC testing are presented in Table 20.

During seal operation, impurity levels in the sodium and cover gas did indicate contamination of the sodium environment with seal leakage oil. Oil leakage samples also confirmed the presence of sodium vapor in the leakage basin.

Sodium analysis results for Cl and Fl are plotted in Figures 30 and 31, showing the sodium temperature history and shaft speed for reference. Sodium and oil vapor concentration in the seal annulus would be expected to be a function of sodium and leakage oil temperature. Concentration of the halogens in the sodium increased with time and sodium temperature up to a period in early October when a down step in the fluorine trend occurred. In mid-November, while still operating over a 1200°F sodium pool, chlorine contaminant levels also showed a decrease. These occurrences can be explained in part by formation of a sodium deposit in the shaft annulus which was found following disassembly. With a deposit of sodium being formed, selective gettering of the oil vapor would occur on this deposit, reducing the amount of oil vapor reaching the sodium pool. Reactivity of fluorine with sodium is higher than chlorine, supporting the earlier step reduction in the fluorine reaching the pool. This trend step also indicates that the contaminants were being trapped out in the system cold trap as they were being introduced, but at a rate less than the rate of introduction.

Figure 32 presents a relative plot of selected reaction product gas as analyzed from periodic cover gas samples from the shaft annulus below the seal. At the initiation of seal testing, the concentration of reaction product was established at a base value 1 unit of chromatograph peak height. As

TABLE 20

COMPARISON OF FERMI AND PSTF OIL VAPOR-SODIUM VAPOR REACTION PRODUCTS
FOUND BELOW THE INBOARD SEAL*

Sample		Contaminant Concentration (%)							
Number	Location	NaCl		NaF		Free Oil		Carbon and Metallics	
		Fermi	PSTF	Fermi	PSTF	Fermi	PSTF	Fermi	PSTF
1	1 in. below scored region on shaft	NA [†]	10	NA	56	NA	0	NA	>1
2	1 in. above scored region on shaft	NA	62	NA	34	NA	0	NA	14
3	7 in. down from shaft step on shaft	NA	56	NA	41	NA	0	NA	11
4	1 in. down from shaft step on shaft	NA	77	NA	32	NA	0	NA	11
5	1 in. up from shaft step on shaft	17	19	34	10	0	21	7	3
6	From leakage basin behind labyrinth	NA	12	NA	6	NA	53	NA	4
7	ID surface of inboard stationary seal	1	9	0	21	70	42	28	13
8	Thermal barrier sleeve	NA	6	NA	15	NA	0	NA	6

*Maximum sodium temperature: Fermi - 1000°F, PSTF - 1200°F

†Not available

LMEC-34035

Figure 30. Sodium Sample – Chlorine Impurity

LMEC-34036

Figure 31. Sodium Sample - Fluorine Impurity

Figure 32. Cover Gas Sample – Oil/Sodium Vapor Reaction Product

leakage developed and sodium temperature was increased, the concentration increased exponentially. A period of high sodium temperature when shaft speed was reduced to zero showed an exponential decrease in concentration as a result of the greatly reduced seal leakage during the static period. It can be concluded that reaction product formation is a function of quantity of oil vapor in the annulus, that is, dependent on shaft speed (leakage) and annulus temperature (sodium temperature). Detection of oil and sodium vapor interaction once a traceable reaction product or products is known has been successfully demonstrated.

By sampling the leakage oil from the inboard seal, which by design drains down into a catch basin exposed to sodium vapor, the approximate amount of sodium vapor present in the chamber can be obtained. Figure 33 presents the history of sodium impurity obtained from the seal leakage oil. The sodium concentration is apparently time and system temperature dependent when viewing the overall contaminant trends. Curve variations are unpredictable but appear to be a function of exposure time of the leakage oil in the leakage basin. The velocity of flow through the basin would determine the exposure time which is, in turn, dependent on leak rate.

D. POST-TEST INSPECTION RESULTS

1. Vapor Deposition Inspection

When the seal assembly was initially removed, a sodium vapor deposition check was made of the thermal barrier region of the test assembly. Vapor deposition was limited in quantity and typical of that found in other LMEC test systems. Figures 34 and 35 provide a visual record of the deposits present. Figure 34a is an overall view of the lower five plates (1 through 5 — 700° to 1050°F) showing typical thin deposits, crazed in appearance, representative of the elevated temperature-limited wetting deposit. Figure 34b is a closeup of plates 3 through 5. This region is significant since deposits found in the shaft annulus in this temperature region were quite different. Deposits formed on plates 5 through 9 are shown in Figure 35. These deposits, bead and viscous droplet forms, are also typical of deposits found in other LMEC test systems.

Figure 33. Leakage Oil Sample –
Sodium Impurity

LMEC-71-7

LMEC-P43014

a. Plates 1 through 5

LMEC-P43011

b. Plates 3 through 5

Figure 34. Sodium Deposits — Lower Thermal Barrier Plates

LMEC-71-7

LMEC-71-7
98

LMEC-P43013

Figure 35. Sodium Deposits — Upper Thermal Barrier Plates

There was no evidence of Fluorolube oil contamination (dark deposits) on test system parts protruding below the thermal barrier region (Figure 36). Physical signs of oil contamination of the system were most severe in the thermal barrier region and above. Figure 37 shows the inside diameter surface of the thermal barrier sleeve at the lower extreme. This is the same temperature range represented by Figure 34a (700 to 1050°F). These deposits are massive and contain regions of dark discoloration not typical of sodium vapor deposits. Because of shadows, the photograph exhibits a bit more dark area than actually existed; however, the farther up the annulus, the darker was the deposit surface. Samples were obtained from this deposit and other regions up in the shaft annulus and leakage basin. Oil-sodium reaction products were determined for this sampling as shown in Table 20, Sample 8. The sample contained the lowest amounts of sodium fluoride (15%) and sodium chloride (6%) relative to the quantity of unreacted sodium present (74%). This contamination level was sufficient to stabilize the deposit on the vertical surface at 1050°F.

At the uppermost extreme of this deposit, a hard dense crust had developed. The crust had built up, making contact with the shaft and scoring it severely. The temperature region represented (700° to 1050°F) falls within the range required for short-term liquid sodium wetting to occur. It can be assumed that the deposit was initiated on a wetted surface. The sharp definition of an upper extreme is attributable to a lack of wetting and the initiation of the crust on the uppermost extreme of the wetted surface. It is the uppermost extreme that is exposed first to the oil vapor as it permeates the shaft annulus. The sodium wetted surface would act as an oil vapor getter since the reactivity of the two materials is violent in nature, and instantaneous at those temperatures. In the small quantities present at any instant, the exothermic reaction would be minimal. Normal sodium deposits developing on a wetted surface in a clearance of 0.35 in. would have run off. Oil contamination made the deposit more stable at these temperatures.

System contamination with sodium-oil vapor reaction products above this initial deposit is itemized in Table 20. Samples (Nos. 1 and 2) were removed from the shaft outer surface just below and above the scored section, respectively (Figure 38). There was no unreacted oil present. Sample No. 1 had

LMEC-P43001

Figure 36. Shaft Surface Below Thermal Barrier Plates

LMEC-71-7

LMEC-P43003

Figure 37. Deposit on Inside Diameter of Thermal Barrier Plate Sleeve

LMEC-P43015

Figure 38. Shaft Contamination Showing Scored Region

LMEC-71-7

the lowest sodium chloride level and the highest sodium fluoride level of all samples obtained. These deposits were soot-like in texture. The surface of the shaft was difficult to clean, reflecting the degree of impregnation of the stainless surface by the reaction products in this region. Three samples (Nos. 3, 4, and 5) were obtained higher up in the annulus at levels 7 in. and 1 in. below the shaft step (diameter change) and 1 in. above the shaft step (Figure 39). Deposits below the step were also soot-like in nature, being free of unreacted oil. The sample above the step introduced 21% free oil, reflecting the lower relative concentration of sodium vapor to oil vapor, and the reduced temperature which reduces the rate of reactivity of the oil vapor with the cooler sodium vapor. Sample No. 6 was obtained from the catch basin (Figure 40). This sample contained the highest amount of free oil with a proportionately low percentage of sodium halides. Sample No. 7, removed from the inner diameter of the inboard stationary ring, contained the highest metallic impurity level, since the ring was directly in the path of leakage oil flow as it left the contacting wear surfaces.

2. Seal Inspection

The inboard seal faces exhibited higher wear than the outboard faces (Figure 41). Wear in both cases was nonuniform, indicating that the bimetallic rotating faces had distorted thermally, causing an overall concave wear surface. This distortion was more severe for the inboard faces, which would indicate that they had been operating at a higher temperature than the outboard faces. The rotating faces use Stellite No. 1 hard facing on a Type 316 SS body. The coefficients of thermal expansion are approximately 5.8×10^{-6} and $8.9 \times 10^{-6}/^{\circ}\text{F}$ for the Stellite and stainless steel, respectively. Uniform heating of the face would produce the concave type of physical distortion realized during the test.

LMEC-P43006

Figure 39. Shaft Contamination at Shaft Step

LMEC-71-7

LMEC-P43009

Figure 40. Contamination of Oil Leakage Catch Basin

LMEC-71-7

Figure 41. Seal Wear

V. SPTF CONTROL VALVE WATER CAVITATION TESTS
(LMFBR Program Plan 3-11, 3-11.5, 3-11.5.3)
S. L. Pfahler

A. INTRODUCTION

The purpose of the water cavitation tests^{*} is to determine the flow and cavitation characteristics of butterfly valves similar to Crane Co. design for the Sodium Pump Test Facility (SPTF) and confirm the valve design for the SPTF main flow loop (MFL) flow control station.

B. DESCRIPTION OF TEST LOOP

The test are being performed at the Rocketdyne High-Flow Test Laboratory at Canoga Park, California. Photographs of the test section are shown in Figures 42 through 45.

Water flow through the test loop is supplied by four pumps rated at 6000 gpm and 600-psi discharge pressure which take suction from a 200,000-gal supply pit. The pumps can be operated in either series or parallel mode. Flowrate is measured by a 20-in. AC EM flowmeter.

The test loop is constructed of 3/8-in. wall, 18-in.-diameter steel pipe. Two 18-in. butterfly test valves fabricated by Mosser Industries to geometrically simulate the SPTF valves are installed in the loop with 6-pipe-diameter spacing between the valves. A single hydraulically operated butterfly valve is installed in the loop at the discharge pit water level to permit regulation of back pressure at the test valves.

Pressure and temperatures are measured at points upstream and downstream of each valve. Valve disk positions are measured by a pointer indicator attached to each valve and by potentiometers attached to the valve shafts for remote readout. Accelerometers are attached to each valve body and are read out on a dual-trace oscilloscope. In addition, a single accelerometer is attached to the pipe downstream of the valve under test and the output is displayed on a spectrum analyzer. Pressure, temperature, flowrate, and valve positions are

^{*}TPL-10-SV-001, "SPTF Control Valve Cavitation Test," LMEC Report, Rev. C (May 5, 1971)

Figure 42. Water Cavitation Tests — 18-in. Test Loop

LMEC-P44007

LMEC-71-7

LMEC-P44008

Figure 43. Water Cavitation Tests — 18-in. Butterfly Test Valves

Figure 44. Water Cavitation Tests — Test Loop Discharge Section

LMEC-P44009

LMEC-P44010

Figure 45. Water Cavitation Tests — Discharge Valve
and Pump Suction Pit

recorded on an 18-channel oscillograph. Photographic records are taken of the spectrum analyzer and oscilloscope data.

In addition, pressure, temperature, flowrate, and audio noise (DB) level measurements are recorded on data sheets at each test point. Figure 46 is a photograph of the recording instrumentation used in the test series.

C. TEST RESULTS

1. Head Loss Coefficient and Hydraulic Interference

A series of tests were performed to determine the test valve head loss coefficient (K factor) and to establish if the 6-pipe-diameter space between the valves was adequate for the prevention of hydraulic interference.

An initial set of tests was performed with the upstream (No. 1) valve at 10, 20, 30, 40, 50, 60, and 70-degree open positions and the downstream (No. 2) valve maintained at the 90-degree (fully open) open position. Head loss coefficients were obtained for the upstream valve from these tests.

A second set of tests was then performed to determine head loss coefficients for the downstream valve with the valve set at positions a factor of 1.10 further open than the upstream valve. The 1.10 factor was recommended as an optimum ratio by Crane Co. for preventing cavitation in the SPTF MFL control valves.

The following head loss coefficients were obtained from the above tests:

<u>Degrees Open</u>	<u>Valve No. 1</u>	<u>Valve No. 2</u>
10	3020	2830
20	346	332
30	78.8	80.4
40	25.5	26.3
50	9.1	9.1
60	3.62	3.46
70	1.33	1.30

The basic equation for head loss through a valve is:

$$\Delta H = K \frac{V^2}{2 g_c} ,$$

Figure 46. Water Cavitation Tests — Recording Instrumentation

LMEC-P44011

where:

ΔH = Head loss through valve (ft of flowing fluid)

K = Head loss coefficient (dimensionless)

V = Mean velocity of flow (ft/sec)

g_c = Gravitational constant (lb-ft/lb force sec²)

Interference, as determined from the effect of the upstream valve on the downstream valves head loss coefficient, is not in evidence from the above data since the curves for both valves are within the error ban.

A summary plot is given in Figure 47. The original curve, as estimated by Crane,* is shown for comparison.

2. Incipient Cavitation Index

A test series was performed to determine the incipient cavitation index for the butterfly test valves.

The incipient cavitation index was determined by plotting the peak output level of the spectrum analyzer at the 150-kHz frequency vs fluid flow for each valve position. The point where the plot of the spectrum analyzer peak vs flowrate begins to increase sharply was taken as the point of incipient cavitation for that valve position. The 150-kHz frequency was selected to allow consistent data reduction for each test.

Incipient cavitation indexes obtained from the above tests are as follows:

<u>Degrees Open</u>	<u>Valve No. 1</u>
10	1.20
20	1.05
30	1.25
40	1.45
50	1.55
60	2.15
70	2.55

*CR-0208, "SPTF Valve Interim Preliminary Engineering Report," Crane Report (July 1, 1970)

LMEC-44012

Figure 47. Water Cavitation Tests - "K" Factor vs Valve Position

The basic equation to determine cavitation index is:

$$\sigma = \frac{P_3 - P_v}{P_1 - P_3 + V^2/2g_c} ,$$

where:

σ = Cavitation index (dimensionless)

P_1 = Valve upstream head (ft)

P_3 = Valve downstream head (ft)

V = Fluid velocity (ft/sec)

g_c = Gravitational constant (32.2 lb-ft/lb force sec²)

P_v = Test fluid vapor pressure (ft)

Incipient cavitation indexes as measured by these tests are not the same as the critical or choking cavitation index reported by Crane.* As defined in this report, incipient cavitation is the initial onset of cavitation. The critical cavitation index is the point where flow cavitation is sufficiently developed to cause the valve flow characteristics to change. The slope of the flow vs the $\sqrt{\Delta P}$ curve changes at this point. Choking cavitation occurs at the pressure drop condition where no increase in flow is obtained by further reduction in downstream pressure with the upstream pressure constant.

Figure 48 is a plot of the incipient cavitation index vs valve position as determined from the above tests. The critical cavitation index used by Crane* is shown for comparison.

3. Cavitation Erosion Damage

A cavitation erosion damage test conducted at the incipient cavitation indexes obtained from the previous tests revealed no damage on the 1100-0 aluminum sleeve detector inserted in the pipe immediately downstream of the test valve with the incipient cavitation condition maintained for a period of 15 min. A

*Ibid., CR-0208.

LMEC-44013

Figure 48. Water Cavitation Tests – Incipient Cavitation Index vs Valve Position

single test conducted under severe cavitation conditions revealed serious localized damage along with less serious general damage over the entire surface of the sleeve detector.

Planned tab damage loss rate tests were not performed because of the difficulty experienced in preventing the hydraulic forces from tearing the sleeve detector loose. It was estimated that the number of retaining screws required to hold the aluminum tabs in place would lead to erroneous damage results. This problem coupled with the uncertainties surrounding damage locations, the exposure time necessary to obtain detectable damage, and the interpretation of data resulted in a decision to terminate further damage tests at this time.

4. Upstream Pressure Effect on Incipient Cavitation Index

Tests conducted at the incipient cavitation index, but with several levels of increased upstream pressure, have revealed that the incipient cavitation index (as established in previous tests) apparently increases with an increase in upstream pressure. These test results tend to verify the observations reported recently by others; however, satisfactory physical explanation of this phenomenon has not as yet been advanced.

D. CONCLUSIONS

The first series of tests verified the adequacy of the 6-pipe-diameter spacing between the butterfly valves. However, the change in the head loss coefficient (over that which was predicted by Crane Co.) resulted in a predicted marginal cavitation performance at the designed 15,000-gpm flowrate condition when Crane Co. critical cavitation indexes were used for the prediction of cavitation.

Analysis of the SPTF MFL operation, utilizing the head loss coefficient and incipient cavitation indexes obtained from these tests, has revealed that in addition to the four control valves, an orifice combination will be required for SPTF operation if the valves are to operate out of the incipient cavitation region.

An increase in the value of the incipient cavitation index with increased upstream pressure would require the addition of other pressure-reducing devices for operation of the SPTF MFL without cavitation. Tests to verify the validity of the reported effect of upstream pressure are currently being planned.

VI. INSTRUMENTATION TESTING, EVALUATION, CALIBRATION, AND STANDARDS

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

C. W. Griffin

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 calibration instrumentation developed under the LMFBR program. The project will also provide for testing and evaluation of commercial instrumentation and for their adoption 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. SODIUM LEAK DETECTOR PERFORMANCE EVALUATION

1. Objective

The objective of this test program is to provide data for the design of sodium leak detection systems for use in the FFTF primary system, based on the evaluation of the performance of several selected detectors. This effort was in response to a test request from WADCO.

2. Approach

The test consisted of observing and recording data on the behavior of leaking sodium and the response of several types of sodium leak detectors. Since no standard, widely accepted leak test apparatus or leak detectors were readily available, they were designed specifically for this test program.

The range of conditions over which test data were required were:

Sodium leakage rate (gm/hr)	1.5 to 160
Sodium temperature (°F)	400 to 1050
Ambient atmosphere:	
Oxygen (%)	0.5 to 2
Hydrogen (%)	0.0 to 2
Dew point, moisture (°F)	-45 to +45

The detectors were tested over the specified range of test conditions and over a range of sampling or sensor placement conditions to determine the detector response and factors which influence the response.

3. Test Apparatus

The test apparatus simulated a length of FFTF primary pipe encased in a typical length insulation module. In this apparatus, liquid sodium was forced at specified flowrates through a hole into a gas annulus formed by the pipe and the surrounding insulation structure. The insulation structure consisted of a stainless-steel sheath covered with a layer of thermal insulation. The sensors or sample lines were mounted on the sheath. A window in the sheath over the leak hole provided visual observation of the leaking sodium.

4. Description of the Detectors

a. Hydrogen Detector

The use of a hydrogen sensor as a sodium leak detector is based on the generation of hydrogen as the result of the reaction of water (moisture) in the environmental gas with the leaking sodium. The sensor used was a Vac-Ion Gas Analysis System, Model No. F-4050-X designed and manufactured by the Varian Vacuum Division of Varian Associates.

The general operating principle is as follows: An evacuated tube, closed at one end by a palladium membrane, is placed in the carrier gas stream. The tube contains an electron emitter and anode. Palladium, being permeable to hydrogen, permits the passage of hydrogen into the electrode region. The hydrogen passed is ionized in the electrode region and the resulting ion current change is the indicator of hydrogen.

The hydrogen detector was connected to the test apparatus through an unheated, 12-ft-long, 1/4-in. by 0.035-in. wall stainless-steel tube. In most tests, the sample point was 9 in. downstream from the leak hole. Other sample points along the length and circumference were evaluated.

b. Liquid Sodium Type Detectors

An electrical contact sensor and an eddy-current proximity sensor were placed in the flow path of leaking sodium and tested.

The electrical-contact sodium detector consisted essentially of a pair of simple short electrodes. They are part of an electric circuit and are placed in the expected path of leaking liquid sodium. The detector is activated by sodium bridging the gap between the electrodes, completing the electrical circuit. Such detectors are not commercially available as sodium leak detectors, but it is common practice to use either of two commercial items: (1) spark plug or similar device, or (2) thermocouple without a junction. For these tests a commercial thermocouple (Conax Cat. No. K-SS25-B-B2-MK-250-A-4) with the junction cutoff was used. This was a chromel-alumel, exposed-junction, stainless-steel-sheathed magnesia-insulated thermocouple. The mounting fitting was screwed into the receptacle provided in the test apparatus trough so that only the electrodes protruded into the trough.

The eddy-current sodium detector consisted of a high-frequency energized inductor placed in the expected path of leaking sodium. Sodium, entering the electromagnetic field about the inductor, adsorbs electrical energy (by eddy-currents). The electrical circuits are designed to respond to the energy change. Eddy-current sensors, available commercially as proximity sensors, can function as sodium leak detectors if they can withstand the high temperature involved. The limited usage of eddy-current type sodium leak detectors involves only models which have been custom designed and fabricated. An eddy-current sodium leak detector which is being developed by the Idaho Nuclear Corporation (INC) was used in these tests.

c. Sodium Vapor Detector

The sodium vapor detector consisted of a sodium vapor responsive sensor through which the leak region environmental gas is forced by differential pressure. There is no commercial instrument of this type designed for sodium leak detection. However, several commercial instruments used for general analysis can perform this function and are available. To avoid the complexity and corresponding cost of the more general analyzers, custom designed and fabricated sodium vapor leak detectors made of commercial components are usually used. For these tests, an atomic absorption sodium vapor detector, whose design is essentially the same as one designed and used at the Argonne National Laboratory (ANL), was used. The light sensor is a part of the available commercial instrument assembly. The other components were sized and fabricated by LMEC.

The light source was a hollow-cathode sodium lamp, Westinghouse type No. WL-23059A mounted in a supporting enclosure. The power was supplied by a Jarrel-Ash Power Supply Model 82-385. The gas sample cell consisted of a stainless-steel block with three passages for gas and the traversing light beam, heaters to prevent condensation of vapor, and thermocouples to measure the cell temperature. The light sensor was a photomultiplier tube mounted in a housing which is a part of the power supply and amplifier. The power supply amplifier unit contains a meter which indicates the intensity of the light reaching the cell.

Light source and sensor were mounted on a sliding frame to permit surveillance of each cell. By this means, "zeroing" to eliminate possible errors due to any of the possible degrading factors could be eliminated and two samples could be sequentially examined or one of these could be a standard gas for calibration purposes.

d. Sodium Vapor Reactant Plugging Type Detectors

Plugging detectors were not included in the original test plan. The introduction of this method came about as the result of inadvertent plugging of the needle valves and flowmeters (Rotameters) in the gas lines to the hydrogen detector and sodium vapor detector.

When it became apparent that a restriction in the gas line and a flow indicator constituted a leak detector about as responsive to sodium leaks as any other detector, a specific model was made and tested for this purpose. This consisted of a 1/4-in. stainless-steel tube, flattened along 5/8 in. of its length and installed on one of the environmental gas exit stubs. The size and shape of the gas passage was simply that which formed after the flattening pressure was relieved. The gap formed was of the order of a few thousandth of an inch. Two other models were made: one used a 1/4-in. tube flattened over a length of 4-7/16 in.; the other a 1/8-in. tube flattened over a 1-1/4-in. length. In addition, another configuration was made of a tee fitting in which a restriction in the tee was formed by a rod mounted in one leg protruding into the flow passage through the other two legs. This device was tested with two different sizes of clearance passages (adjusted by the rod position), 0.005 and 0.009 in.

e. Chemical Detectors

Chemical detectors were not included in the original test plan. The introduction of this method came about as the result of the failure to obtain the proper response with the sodium vapor detector and the need to determine how much sodium of any form was actually reaching the vapor detector.

Initially, a sample of the environmental gas was bubbled through a phenolphthalein solution to detect either sodium vapor or airborne sodium oxide particles. Although phenolphthalein will discolor, it varies with the concentration of the solution. This depended upon chemical analysis for evaluation of sodium content. Gas volume flow-through was measured very carefully to determine the average sodium concentration in the gas during the duration of the test. Good response led to continued use of the phenolphthalein solution during the remainder of the testing program. After completion of each test, the phenolphthalein solution was analyzed in the laboratory for quantitative data on the amount of sodium in the environmental gas. The laboratory analysis greatly increased the sensitivity of this method and eliminated the uncertainties involved in detecting the appearance of a pale color. This detector was not regarded as an on-line leak detector. Subsequently, on-line detection was tested using a pH indicator. A Beckman Research pH meter with a nonselective ion electrode was used. This instrument provided a continuous indication of the sodium in the environmental gas.

5. Testing Program

Testing began on April 1, 1971, following fabrication completion of the test apparatus, acquisition of the test detectors, and acceptance of the plans and procedures. Initially, two shakedown tests were conducted to provide operational experience and to ascertain the difficulties to be expected. The first of these shakedown tests was under the least troublesome conditions, and the second was under the most troublesome conditions.

Upon completion of these shakedown tests, a complete testing program was devised to subject each type of detector to (1) the condition believed to be the most favorable for the detector, then (2) the condition believed to be the least favorable to the detector operation. The plan provided simultaneous test of several detectors for each test condition so that a comparative evaluation could be made.

6. Test Results

Sixteen tests were performed; a summary of the results of these tests is presented in Table 21. The order of the entries in this table is first by temperature, next by environmental gas content, oxygen, and water, respectively. The time listed is the time during which sodium was leaking and instruments were operating to detect the leak.

TABLE 21
SODIUM LEAK TEST SUMMARY -
SODIUM LEAK DETECTOR PERFORMANCE TEST

Test No.	Temperature (°F)	Leak Rate (gm/hr)	Total Leak (gm)	Time (min)	Oxygen Concentration	Moisture Concentration Dewpoint (°F)	Detector Response									
							Hydrogen	Electrical Contact	Eddy-Current	1/4 in. by 5/8 in. Plugging	1/4 in. by 4-7/16 in. Plugging	1/8 in. by 1-1/4 in. Plugging	Restricted Tee Plugging	Phenolphthalein	pH	Sodium Vapor
11	400	(1)	40.0	105	0.5	-45 to +45	No	(6)	(5)	(5)	No	(5)	No	Yes	(5)	(5)
8	400	1.5	4.5	180	1.0	-45	No	(6)	(5)	(5)	No	(5)	No	Yes	(5)	(5)
3	400	1.5	8.4	189	2.0	-45	No	(6)	(5)	No	(5)	(5)	No	Yes	(5)	(5)
4	400	(1)	44.0	65	2.0	+45	Yes	(6)	(5)	Yes	(5)	(5)	Yes	Yes	(5)	(5)
13	500	(1)	49.4	59	1.0	-45 to +45	No	(6)	(5)	(5)	(5)	No	No	Yes	Yes(4)	(5)
14	550	(1)	6.0	65	1.0	-45	Yes	(6)	(5)	(5)	(5)	Yes	Yes	Yes	No(4)	(5)
12	600	(1)	36.3	26	0.5	-45	Yes	(6)	(5)	(5)	(5)	Yes	Yes	Yes	Yes	(5)
10	600	(1)	16.3	40	1.0	-45 to +22	Yes	(6)	(5)	(5)	Yes	(5)	Yes	Yes	(5)	(5)
15	700	(1)	40.0	46	0.5	-45	Yes(4)	(6)	(5)	(5)	(5)	Yes	Yes	Yes	Yes	(5)
9	700	(1)	15.4	37	1.0	-45	Yes	(6)	(5)	(5)	Yes	(5)	Yes	Yes	(5)	(5)
16	700	(1)	94.0	54	1.0	-45	Yes	(6)	(5)	(5)	(5)	Yes	Yes	Yes(4)	Yes	(5)
6	800	160	77.0	30	0.5	-45	(5)	(6)	Yes	Yes	(5)	(5)	(5)	Yes	(5)	No
5	1050	150	32.0	12	0.5	-45	Yes	(5)	Yes	Yes	(5)	(5)	Yes	Yes	(5)	(5)
7	1050	160	37.0(2)	14	0.5	-45	(5)	(6)	(5)	Yes	(5)	(5)	Yes	Yes	(5)	No(7)
2	1050	(1)	(3)	45	0.5	-45	Yes	(5)	(5)	(5)	(5)	(5)	(5)	(5)	(5)	No(7)
1	1050	160	82.0	37	0.5	+45	Yes	Yes	(5)	(5)	(5)	(5)	(5)	(5)	(5)	No

Key:

- (1) Not controlled, generally a spurt or series of spurts.
- (2) An additional unmeasured amount of sodium leaked prior to initiation of controlled leak.
- (3) No reasonably accurate measurement.
- (4) Sensor mounted at end of 50-ft unheated transmission line.
- (5) Detector not installed or not operated.
- (6) Test duration not long enough to enable operation.
- (7) Responded to sodium oxide particle deposition, not as vapor detector.

7. Conclusions and Recommendations

The results of these tests engender the following conclusions and recommendations pertinent to the FFTF applications as indicated in the test request:

- 1) The only type of detector which performed satisfactorily over the entire range of conditions, including operation at the end of a 50-ft transmission line, was the chemical type. Although this performance was verified only for the phenolphthalein indicator, other chemical indicators having the same performance governing factors will provide substantially the same detecting capability. The pH indicator, tested over a narrower range, can be expected to perform satisfactorily over the entire range. The advantage of the pH indicator is that it is a commercially available instrument, can be used for on-line operation, and by the use of a sodium electrode can be made responsive to sodium only. Consequently, leak detection by gas sampling and sensing by a sodium ion electrode pH indicator is a recommended method when slow time response and extensive sampling is acceptable or where leaks in low-temperature systems must be detected.
- 2) The plugging detector performance was satisfactory over a wide range of conditions with failure only at the low temperatures, 500°F and lower. On the basis of performance, the plugging detector is suitable for applications where leak detection at the lower temperature is not required. Subject to this limitation, the plugging meter is recommended when cost, installation, and other factors favor the use of plugging detectors over the pH indicator.
- 3) Since the pH indicator and the plugging detector are based on gas sampling, they have much in common and a system based on a combination of these instruments may offer significant advantages. A plugging detector which is relatively economical and can be mounted as close to the sodium system as desired can be placed in each gas sample line with its gas flow monitoring system. For low-temperature mounting, the gas sample lines can be valved through a branch system to a single pH indicator, which is a relatively expensive instrument and cannot be mounted as close to the sodium system as the plugging detector.

- 4) The detectors which depend on sensing liquid sodium (electrical contact and eddy-current types) should be used only in applications in which the gas methods discussed above are not feasible or where the conditions are clearly more favorable to the use of these detectors. The eddy-current type is the preferred form since it is not susceptible to the false alarms characteristic of the contact type and the means of verifying its proper operation is more nearly satisfactory.
- 5) The hydrogen detection method is not recommended for primary systems since it cannot function in the inherent tritium background required. The detector could be very promising, however, for secondary systems if the high oxygen content of air did not deter reaction. No tests were conducted in air since its test criteria were a maximum of 2% oxygen.
- 6) The vapor method is not recommended since the oxygen, hydrogen, and moisture environment form reaction products which are not sensed by the vapor detector.

C. SURFACE (SKIN) TEMPERATURE MEASUREMENT THERMOCOUPLES - LMEC INSTALLATION

The normal method for measuring preheat temperatures or surface (skin) temperatures of sodium system piping and components at LMEC has been with wire-type thermocouples tack welded to the surface. The thermocouple material used was 18 to 22-gage type J or K thermocouple wire insulated with enameled asbestos/fiberglass. The attachment to the surface and thermocouple "hot" or measuring junction was made by either TIG welding or capacitance discharge welding.

This material is available from a large number of suppliers, with or without a metallic overbraid, and is used extensively for this application throughout industry. However, the material is limited to applications where the measured temperature does not exceed 950°F. At temperatures above 950°F, the enamel and binders burn off and the insulation becomes very powdery and drops off the thermocouple conductors.

Major manufacturers of thermocouple wire were contacted and recommendations solicited for a wire-type thermocouple material that would withstand 1250°F continuous operation. The recommendation was a fiberglass/fiberglass

material that contains no impregnants or binders and is good to 1800°F. This material is very fragile and susceptible to abrasion. A metallic overbraid was not recommended to protect the fiberglass since bending caused some of the overbraid strands to break and penetrate the fiberglass, thus short circuiting the thermocouple conductors.

One system, PMIS in Building 032, was instrumented using the fiberglass / fiberglass insulated thermocouple material. It was found that extreme caution must be exercised in installing the thermocouples and thermal insulation to avoid damage to the thermocouples.

An analysis was conducted to determine the accuracy of the measurement if a ceramic-insulated, metal-sheathed thermocouple was attached by binding or metal clips to the component. Since the junction was not in contact and it could not be assured that the tip of the sheath would always be in contact with the component, it was concluded the error could be as much as 20 or 30°F. Further analysis was conducted and it was determined that the measurement error could be reduced to less than 5°F if a pad were welded or brazed to the thermocouple sheath and in turn tack welded to the component. This could be further ensured if the tip of the sheath protruded slightly below the undersurface of the mounting pad.

The result of this investigation was the preparation of LMEC Specification 7693-CE12100-2, "Skin Thermocouple, Pad Mounted, Stainless Steel Sheathed, MgO Insulated, Type K." Thermocouples procured to this specification were first used on the sodium leak detection experiment test apparatus, and are currently being installed on the ITR. They are being ordered to replace failed thermocouples on PMIS.

The pad-mounted sheathed thermocouples offer the following advantages:

- 1) Are very easy to install.
- 2) Are very rugged and are seldom damaged during installation of the thermocouples or thermal insulation.
- 3) May be calibrated prior to installation if required.
- 4) Are long-lived and reliable, thus reducing the number of redundant thermocouples required.

As a result of the cited experiences at LMEC, pad-mounted sheathed thermocouples are used for all surface temperature measurements where the temperature is expected to exceed 900°F, or for critical safety or control systems. In the interest of economy, enameled asbestos/fiberglass wire-type thermocouples are used for monitoring purposes where temperatures are less than 900°F. High-temperature fiberglass/fiberglass are used only for special cases where extreme care and control can be exercised in their installation and use, and for applications where pad-mounted sheathed thermocouples cannot be installed.

D. HIGH-TEMPERATURE STRAIN GAGE DEVELOPMENT PROGRAM
(LMEC Contract No.N083-0004, Battelle Memorial Institute, Columbus Laboratories)

The objective of this program is to develop a resistance-type strain gage for use in air and/or sodium environment at temperatures up to 1200°F. The program is divided into three phases: (1) Phase I is for research and development, (2) Phase II is to cover fabrication and testing, and (3) Phase III is the follow-on program to develop gages capable of being used in a radiation environment, of operating at 1400°F. Only Phase I is presently authorized.

Wire Materials Preparation (Task 2)

The purpose of this task is to develop, test, and fabricate into strain gage wire, two alloys with electrical characteristics suitable for use as strain gages at 1200°F.

The two alloys designated BCL-3 and BCL-5 were drawn to 0.002- and 0.001-in.-diameter wire during this period. The drawing characteristics of these alloys were excellent with no breakage attributed to metallurgical problems.

A 1-ft length of nominal 3/4-in.-diameter forged bar of each alloy was used for the wire drawing. These rods were rolled at 1800°F to rods 5/8-in. diameter, followed by machining to approximately 1/2-in. diameter to remove any surface defects. The machined rods were rolled at 1700°F to a diameter of 29/64 in., then the temperature was reduced to 1600°F and the rods were rolled to 9/32-in. diameter. The rolling behavior of both alloys was very good.

The rods were swaged at 1500° to 1600°F to 0.175-in. diameter with no difficulty. These rods were then stress relieved at 1600°F for 10 min, air-cooled and then centerless ground to a nominal 0.117-in. diameter.

The centerless ground rods were 100% inspected using a binocular scope at 9X magnification. In preparation for wire drawing, the rods were annealed at 1500°F for 1 hr in an argon atmosphere. Some surface oxide was found after the annealing; it was removed and the rods again inspected under the microscope.

The rods were drawn to 0.096-in. wires, then were 100% inspected by radiography. The results indicated that the wires were of excellent quality and free of any significant flaws.

At 0.081 in., the wires were process annealed at 1300°F for 40 min. The annealing temperature was selected after conducting a study of samples annealed at 1200°, 1300°, 1400°, and 1500°F.

The wires were annealed at 0.057-in. diameter at 1200°F for 15 min in vacuum. Both alloys were slightly scored in going through the 0.036- and 0.034-in. dies. These score marks were removed by sanding with 400-grit abrasive paper, and the wires cleaned. The wires were then annealed in vacuum at 1200°F for 15 min and cooled in vacuum. These wires were then drawn to 0.025 in. before annealing again at 1200°F for 15 min in vacuum. Following this anneal, the wires were drawn to 0.0125 in. and annealed again. The next annealing point was at 0.0063 in., followed by a drawing to 0.0031 in. A brief annealing study was conducted at this size. The wire was annealed at 1200°F for 10 min, then drawn to 0.00197-in. diameter when approximately 2400 ft of wire was set aside. The remaining wire was drawn to 0.0016 in., annealed, then drawn to 0.00105 in. to complete the wire drawing operation. There were approximately 2400 ft of each alloy at the 1-mil diameter, and at the 2-mil diameter.

All other tasks in this program were curtailed during this period so that the wire drawing could be completed.

E. HIGH-TEMPERATURE PRESSURE TRANSDUCER DEVELOPMENT PROGRAM (LMEC Contract NOZPLW753000, Bailey Meter Company)

The objective of Phase I of this program is to determine the feasibility of fabricating a fast-response pressure transducer capable of operation at 1200°F in sodium. Analytical and experimental investigations of one or more methods, systems, and materials will be conducted to develop absolute and gage pressure transducers and differential pressure transducers. Bench-scale demonstration units will be fabricated to evaluate the feasibility and potential limitations of the operation of the pressure transducer.

Bailey Meter Company was directed to suspend work on this program in February 1971 because of funding limitations. Work is to resume July 1, 1971.

1. Platinum Element Stability Test (Task A)

The pressure-sensing element is to be a one-piece sapphire structure (capsule). Platinum film strain gages are sputter deposited on the diaphragm portion of this capsule to sense the strains resulting from the applied pressure. The purpose of this task is to test and evaluate various thicknesses of deposited platinum films. Stability of the electrical characteristics of these test resistors is of prime concern.

High-temperature stability of the platinum film test sensors was achieved. Using an alumino-silicate precoat as an interface between the sapphire substrate and the platinum film and as a protective coating over the platinum film, a stability, characterized by a resistor drift rate of only $-0.6 \mu\Omega / \Omega/\text{hr}$ at 1200°F , has been achieved.

The tests were conducted in air, which is a more severe condition than that of the evacuated pressure sensor capsule. The temperature stability of the transducer using matched sensors of this type should be even greater because of the temperature-compensating effects of the half-bridge arrangement.

The results of platinum film resistor tests are presented in the following table:

<u>Test Sample</u>	<u>Thick- ness ($\text{\AA}$)</u>	<u>Resis- tance (Ω)</u>	<u>Time (hr)</u>	<u>Temper- ature ($^\circ\text{F}$)</u>	<u>ΔR (Ω)</u>	<u>Drift (%)</u>
1-12-71-56	125	6544	250	1100	-0.5	0.01
1-12-71-56	125	6753	250	1200	-2.5	0.04
1-21-71-61	125	6193	550	1200	-18.0	0.28
1-21-71-62	250	3138	550	1200	-1.0	0.03

A new test geometry was used for these tests (see Figure 49). The preparation method was as follows: (1) precoat the sapphire test substrate with $3000 \text{\AA}$ of Owens-Illinois type EE10 alumino-silicate, (2) sputter deposit the platinum film, (3) photoresist mask, (4) sputter etch to define the pattern, (5) resist stripping, (6) lead attachment by welding, and (7) overcoating with $1000 \text{\AA}$ of EE10.

Figure 49. New Resistor Test Coupon Geometry – Pressure Transducer Platinum Element Stability Test

2. Sensor Fabrication Process Study (Task B)

The purpose of this study is to establish reproducible fabrication procedures which will allow achievement of satisfactory geometrical and electrical sensor characteristics such as line resolution, adhesion, difference of coefficients of expansion, specific resistivity, temperature coefficient of resistance, tracking, matching, and reproducibility.

Ten pressure transducer capsules were fabricated. Two of the units (2-2-71-02 and 2-2-71-03) were pressure tested this period in the range of 0 to 250 psi at both 80°F and 450°F. The results of the tests of unit 2-2-71-02 are shown in Figures 50 and 51. The resistors were not sufficiently matched to allow for a compression-tension connection. Therefore, the output level is that of a quarter-bridge system. The output level will be increased when proper matching of the resistance elements is achieved and they are correctly positioned to measure both tension and compressive strain.

During these tests it was noted that the "compression" resistor (the element closest to the edge of the diaphragm) was not measuring the calculated strain level. Misalignment of the crystalline axis of the capsule was suspected. An optical technique, using polarized light, was used to determine the degree of misalignment of the crystalline axis. No misalignment greater than 2 deg was found. It is suspected that there could be some reinforcing action created by the small fillet at the juncture of the diaphragm and side wall of the capsule. This will be inspected, using holographic techniques, when work resumes.

Another consideration pertaining to the compression resistor is that of matching its resistance and TCR to that of the tension resistor. Sputtering deposition into the well of the capsule results in a convex film thickness distribution at the bottom of the well. Thus, the tension resistor has a greater thickness than the compression resistor. The proposed solution is to reduce the pressure during sputtering by approximately one order of magnitude. This will decrease the deposition rate somewhat.

The desired resolution of the sensor resistors was obtained by using a small, round, chrome, high-resolution mask which fits into the well and is positioned by means of an adapter. This allows the use of contact masking in the same manner as is done on flat microcircuit substrates.

Figure 50. Deposited-Platinum-Film Strain Gage Pressure Transducer Output vs Pressure at 450°F

Figure 51. Deposited-Platinum-Film Strain Gage Pressure Transducer Output vs Pressure at 80°F

LMEC-71-7

The final test during this period was to determine the burst strength of the capsule. The capsule was designed to operate in the range of 0 to 250 psi with a safety factor of 300%. The capsule was pressurized to 1500 psi before bursting, which may mean that the reported sapphire strength values are too conservative. If this proves to be true (based on further tests) the capsule could be redesigned for higher output (thinner diaphragm).

3. High-Temperature Seal Fabrication (Task D)

The purpose of this task is to define and demonstrate the required integrity of the high-temperature vacuum seal at the base of the capsule. The initial brazing techniques are to be developed using Lucalox, a high-purity translucent form of aluminum oxide, in place of sapphire.

Localized leakage was found on three of four test specimens after being subjected to a thermal soak at 1200°F for 250 hr. Accelerated tests at 1350°F in air did not enlarge these seal flaws. This confirmed the opinion that there is no problem with the Cusil/Cr-Au material but rather that the lapping technique used on the mating surfaces needs improvement.

F. RESPONSE TIME OF SIMULATED LARGE PERMANENT MAGNET FLOWMETER

1. Introduction

The purpose of these tests was to determine the feasibility of a laboratory determination of the response time of large permanent magnet flowmeters. This information is required in the design of the FFTF plant protection system.

2. Test Description

The first test was performed by oscillating a 12-in.-diameter by 3-ft-long aluminum cylinder in the 13-in. magnetic gap of a permanent magnet with 14-in.-square pole pieces and a field strength of about 250 gauss. The aluminum cylinder was oscillated at frequencies between about 6 and 20 Hz, with peak-to-peak displacements ranging from about 0.8 in. at 6 Hz, to about 0.4 in. at 20 Hz. Test data consisted of a signal from an accelerometer mounted on the aluminum cylinder, displacement of the aluminum cylinder as measured by an LVDT transducer, and the emf induced in the aluminum cylinder at right angles to cylinder motion and magnetic flux. The data were recorder on a Brush stripchart recorder. This test setup is shown in Figures 52 and 53.

7694-40421A

Figure 52. Test Setup for Simulated Large Permanent Magnet Flowmeter Response Time Test

Figure 53. Test Instrumentation for Simulated Large Permanent Magnet Flowmeter Response Time Test

To confirm the validity of these preliminary test results, further tests were run in which the 12-in.- and 2-in.-diameter aluminum cylinders were simultaneously oscillated in magnetic fields. The purpose of this test was to determine whether the time lag from the first tests was valid or whether some characteristic of the test setup or instrumentation was giving a false time lag indication. The second tests were judged capable of answering this question since, according to theory, the time lag should be proportional to the square of the diameter of the cylinder. Thus, if the time lag indication is valid, there should be a substantial difference in the time lag of the 2-in. and 12-in. flowmeters, whereas measurement of the same time lag for both sizes would indicate a faulty test method.

3. Theory

Two methods have been proposed for prediction of the response time of permanent magnet flowmeters. One is based on the diffusion time of a magnetic field in a conductor and predicts a response time $t = d^2 \mu / \rho \times 10^{-8}$ where d = diffusion distance (assumed to be pipe diameter), μ = permeability (assumed to be 1 of Na), and ρ = resistivity. For a 12-in. aluminum flowmeter,

$$t = \frac{(12 \text{ in.})^2 (2.54 \text{ cm/in.})^2 \times 10^{-8}}{2.828 \times 10^{-6} \text{ ohm-cm}} = 3.285 \text{ sec} .$$

The second method is based on the time constant of the circulating eddy-currents in the sodium. The magnitude of this time constant may be estimated as follows:

$$t = \frac{L \text{ (inductance)}}{R \text{ (resistance)}} .$$

Assuming a circular eddy-current loop with a diameter equal to pipe diameter (d) and square cross section of $d/2$, the inductance may be estimated as: $L = 2 \pi d \times 10^{-7}$ with d in meters and the resistance as $R = 16 \pi \rho / d$, where ρ = resistivity in ohm-meters. Therefore,

$$t = \frac{2 \pi d \times 10^{-7}}{16 \pi \rho / d} = \frac{d^2 \times 10^{-7}}{8 \rho}$$

$$t = \frac{(12)^2 \times 10^{-7}}{(39.37)^2 \times 8 \times 2.828 \times 10^{-8}} = 0.041 \text{ sec} .$$

4. Results

Figures 54 through 56 show the results of the first test series. Figure 54 is a plot of phase angle lag vs frequency. The phase angle here is the number of degrees from the zero-crossing point of displacement (which is the point of maximum velocity) to the peak of the induced voltage curve. Figure 55 is a plot of time lag vs frequency, where the time corresponds to the phase angle lag of Figure 54. Figure 56 is a plot of normalized peak-to-peak amplitude of the induced voltage vs frequency. The amplitude of the induced voltage must be normalized to mv/in./sec because the induced voltage is directly proportional to velocity. The data below approximately 10 Hz is questionable because of the presence of second harmonic distortion of substantial magnitude in the induced voltage signal. The cause of the second harmonic is not known at this time.

The results of the second test confirmed the validity of the test method; there was no discernible time lag for the 2-in. flowmeter signal (visual examination of chart paper run at 125 mm/sec) while the 12-in. flowmeter time lag was comparable to that determined in the first test.

5. Conclusion

These tests tend to confirm the feasibility of this technique for experimental determination of the response time of a simulated large permanent magnet flowmeter. More sophisticated data acquisition and reduction techniques, utilizing magnetic tape and such tools as power spectral density analysis and appropriate filtering, will undoubtedly improve the quality of the data. These tests also tend to confirm the theory which predicts fast response times for even very large permanent magnet flowmeters. Based on the data from this experiment, it seems reasonable to conclude that the response time of the test simulated flowmeter is not greater than 40 msec. Because of the poorer conductivity of sodium, as compared to aluminum, this would correspond to response time for sodium flow through a 12-in. permanent magnet flowmeter of about 6 and 4 msec at sodium temperatures of 600° and 960°F, respectively.

Figure 54. Phase Angle Lag vs Frequency –
Simulated Large Permanent Magnet Flow-
meter Response Time Test

Figure 55. Time Lag vs Frequency – Simulated
Large Permanent Magnet Flowmeter Response
Time Test

Figure 56. Normalized Peak-to-Peak Amplitude of Induced Voltage vs Frequency – Simulated Large Permanent Magnet Flowmeter Response Time Test

G. TRANSIT TIME FLOWMETER TEST SUMMARY

1. Introduction

The principal objective of the Transit Time Flowmeter (TTF) development program is the demonstration of the measurement of liquid metal flowrate in large pipes by the transit time technique. The TTF involves detecting small random temperature variations (thermal eddies) at two stations in the flow stream and measuring the average transit delay (time). Signals from the two detectors are cross correlated with a variable time delay, and the time corresponding to the maximum correlation is the best estimate of the fluid transit time. Figure 57 depicts this process. A complete mathematical development of the scheme is presented in Reference 1 (listed at the end of this section). Previous work has been done on smaller (2-in. pipe sections) TTF systems in water and sodium.^(1,2) This report presents selected results in water and sodium tests of a 6-in. - diameter system. A final detailed report is being prepared for the complete documentation of the tests.

2. Discussion

The sensor design is depicted in Figure 58. Detailed design information is presented in References 3 and 4. The radial locations of the thermocouples on the sensor are based on specific flow profile considerations and are discussed in Reference 1.

Figure 59 depicts the sensor installed in the flow section.^(2,3) Since this was a research- and development-oriented evaluation test series, several sensors were located along the test section as shown in Figure 60.⁽³⁾

Sensor signals were fed to a system which is depicted in Figure 61 (from References 1 and 2). Data was hand recorded during testing and tape recorded for subsequent analysis after test runs were completed.

The test section was installed at Rocketdyne in the High Flow Test Facility for water tests, and at LMEC in the SCTI for sodium tests (see Figure 62).

3. Test Descriptions

The water test (Rocketdyne) procedure followed a troubleshooting sequence for the first part of the test. The following event sequence occurred during the setup and checkout period:

- 1) Equipment setup

- 2) Pickup noise troubleshooting

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Figure 57. Transit Time Flow by Correlation of Thermal Fluctuations

LMEC-63004

NOTE: ALL THERMOCOUPLES 0.040 in. EXCEPT NOTED

LMEC-63005

Figure 58. Transit Time Flowmeter Sensor Assembly

Figure 59. Cantilevered Thermocouple Sensor in 6-in. Transit Time Flowmeter

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LMEC-63007

Figure 60. Installation of 6-in.-Diameter Transit Time Flowmeter in the SCTI Primary Loop

LMEC-63008

Figure 61. Transit Time Flowmeter Test Instrumentation System

7757-4035

Figure 62. Transit Time Flowmeter Test Installation at Rocketdyne High-Flow Test Facility

- 3) Preliminary on-line velocity and profile measurements
- 4) Heater power check
- 5) Inlet pressure effects
- 6) Cross profiles
- 7) Thermocouple time constant test
- 8) Profile repeat
- 9) Preliminary calibration
- 10) Test configuration setup and checkout
- 11) Heater ΔT test
- 12) Water injection test.

Following the checkout period, data was recorded on magnetic tape at the following operating conditions:

- 1) Heater and profile test
 - a) 17.5 ft/sec
 - (1) 12 to 36-kw heater power
 - (2) Flow profile
 - b) 7.5 ft/sec
 - (1) 12 to 36-kw heater power
 - (2) Flow profile
 - c) 27.5 ft/sec
 - (1) 0 to 48-kw heater power
 - (2) Flow profile
- 2) Calibration runs — 5 to 30 ft/sec
- 3) Off-limits flow — 2.5 and 40 ft/sec
- 4) Vibration test
- 5) Volume tank check
- 6) Sensor variation
- 7) Error analysis

The sodium test (LMEC) procedure is documented as Reference 5. Following setup and checkout of the TTF equipment, data was taken and/or recorded on magnetic tape as described in the following paragraphs.

The SCTI was operated through one thermal cycle for the test. The sodium temperature was stepped through a temperature sequence of 500°, 700°, 870°, 700°, and 500°F. At each temperature the flow velocity was varied in sequence from 15, 25, 5, 10, 20, 30 ft/sec and the maximum attainable value.

At temperatures of 500°, 700°, and 870°F the heat rejection system varied through minimum, partial, and maximum capability to observe the effects on the injector. At 500°F and at maximum heat rejection a 2-fps measurement was taken using the EM pump which tested the TTF with circulating EM pump currents. At the second 500°F plateau the injector was set for minimum heat rejection and insulation was removed from the pipe upstream of the TTF. This operating condition tested the TTF configuration without an injector.

4. Data Summary (General)

Data summarized in this report are limited to flow velocities and dimensionless velocity ratios (relative velocity indication). The fluid velocity values were calculated from the best estimate of the transit times and the known distance between sensors.

An example of a correlation curve as photographed from the oscilloscope is depicted in Figure 63. The peak of this curve is "located" by a calibrated bin pip and read off for each curve. From this indicator the transit time is established.

The velocity values were ratioed to the average fluid velocity to form dimensionless parameters, and statistically analyzed in this form.

The various reduced data statistical summaries presented herein resulted from application of the following considerations:

- 1) The data are represented by normal distributions
- 2) From above, the standard deviation (S) was calculated from:

$$S = \sqrt{\frac{\sum_{i=1}^N (X_i - \bar{X})^2}{N - 1}},$$

LMEC-P63009

Figure 63. Example of Correlation Curve
as Photographed from Oscilloscope

LMEC-63010

Figure 64. Typical Water Sensor Data and Least-Squares Fit Curve

where

$X_i \equiv$ ith data value

$\bar{X} \equiv$ mean of sample

$N \equiv$ number of samples

$\sum_i \equiv$ summation of i values.

3) Maximum expected values (extreme value ranges) where estimated by:

$$\text{Max.}_{(\text{lower})}^{(\text{upper})} = \text{mean} \pm KS$$

where

$K \equiv$ tolerance factor for normal distribution arbitrarily chosen at the 95% tolerance, 0.75 confidence levels

$S \equiv$ standard deviation as defined in item 2.

4) Velocity values were normalized (referenced) to test values in the case of water data and average of data values for the sodium data.

5. Summary of Water Data Analysis

For the water data, all TTF indicated velocities were ratioed to the facility-measured flow velocity. The test facility flowrate was determined by elapsed time and calibrated weight system.

Figure 64 presents a typical plot of the data by sensor pair. Note that the data are trended. Figure 65 identifies sensor location by number. All data values except 2.5 ft/sec were linearly least squares fitted for each sensor pair (6 points per curve at 5, 10, 15, 20, 25, 40 ft/sec).

The standard deviations of the fit residuals yield a measure of each sensor pair's accuracy relative to the Rocketdyne calibration value over the flow range. Note that the variation so expressed does include whatever inaccuracy component is introduced by the calibration facility.

For purposes of establishing reasonable maximum confidence intervals, and based on a sample of 6 points per curve, a "K" factor of 2.9 (95% of the values at a probability of 0.75 would fall within these limits) is applied in Table 22.

(FOR WATER FLOW TESTS AT 17.5 ft/sec ONLY)

(FOR OTHER THAN PRECEDING WATER FLOW TESTS)

(SODIUM TESTS)

LMEC-63011

Figure 65. Transit Time Flowmeter Test — Sensor Locations

TABLE 22

LEAST-SQUARE FIT RESIDUALS OF VELOCITY RATIO FOR
SIX-INCH TTF WATER DATA BY SENSOR PAIR

Pair	Standard Deviation (S)	±Tolerance Interval* (2.9 S)
(1-3) 0.060 in.	0.014	0.041
(3-4) 0.060 in.	0.012	0.035
(4-6) 0.060 in.	0.018	0.052
(1-3) 0.040 in.	0.018	0.052
(3-4) 0.040 in.	0.008	0.023
(4-6) 0.040 in.	0.006	0.017

*Since the "mean" values are close to 1, these values may approximate percentages when multiplied by 100.

TABLE 23

SIX-INCH TTF WATER DATA BY SENSOR PAIR VARIATIONS
OF LEAST-SQUARES FIT AT 20 FT/SEC

Pair	Value at 20 ft/sec	Mean	Standard Deviation (S)	±Limit (2.9 S)
(1-3) 0.060 in.	1.024	1.023	0.024	0.070
(3-4) 0.060 in.	1.025	1.023	0.024	0.070
(4-6) 0.060 in.	1.032	1.023	0.024	0.070
(1-3) 0.040 in.	0.986	1.023	0.024	0.070
(3-4) 0.040 in.	1.021	1.023	0.024	0.070
(4-6) 0.040 in.	1.050	1.023	0.024	0.070

To get some idea of the spread in absolute sensor pair-to-sensor pair values, since the data are trended, a representative value for each pair at a flow of 20 ft/sec (when calculated from the fit curve) was analyzed (see Table 23). Note that the water data values were grouped according to thermocouple type (060/040) and by sensor pairs to arrive at sensor-to-sensor and type-to-type comparisons.

In addition to the sensor-to-sensor data (which includes all similar type thermocouples electrically summed), individual thermocouple data was reduced. This "profile" data was summarized by grouping by thermocouple geometric location

(all were 0.040 in.) and by flowrate levels. Test data values were referenced to facility flowrates (normalized) and differences from theoretical profiles were calculated.

A typical plot of the theoretical and complete data set for the profile data for flow at 17.5 ft/sec is presented in Figure 66. A summary of the variations of the difference from indicated to theory profile flow values is presented in Table 24. Note that the values presented in Table 24 are standard deviations. Maximum expected deviations at the 95/75 tolerance/confidence levels may be obtained by multiplying values at 6.62 and 27.3 ft/sec by 4.2 and the values at 17.5 ft/sec by 3.1.

Observations from the data of Table 24 include the following:

- 1) The consistently least variation is observed from thermocouple numbers 4 to 9 (greatest on number 1).
- 2) The variations for the profile data (individual thermocouples) are generally greater than from the sensor data (electrically summed thermocouples).

Although not included in Table 24, a bias from the theory is apparent from Figure 65. No single thermocouple location exhibits a consistently least bias but thermocouple No. 7 appears closer to the theory values generally.

6. Summary of Sodium Data Analysis

Because of the uncertainty in establishing sodium flows during TTF testing at the SCTI, all flow velocities were ratioed to the average of the data at each level. As a result no analysis regarding a comparison or "accuracy" as a function of flow was accomplished as was done with the water data. However, by grouping the data by thermocouple type (0.040 in. / 0.060 in.) and by operating temperature, the data summary presented in Table 25 was established.

Observations from the data in Table 25 include the following:

- 1) The 0.040-in. thermocouples gave slightly higher flows than the 0.060-in. thermocouples at 700°F and 870°F and when grouped together.
- 2) The variations of the sodium data are typically three times greater than the water data.
- 3) No appreciable variation differences or trends are apparent as a function of temperature.

Figure 66. Transit Time Flowmeter Test -
Water Profile Data at 17.5 ft/sec

TABLE 24

SIX-INCH TTF WATER DATA BY THERMOCOUPLE LOCATION
(Standard Deviation of Difference from Theory for All Sensor Pairs)

Flow Velocity (ft/sec)	Thermocouple Identification Number (see Figure 67)							
	1	2	4	5	6	7	9	10
6.62	0.046	0.018	0.010	0.014	0.032	0.028	0.010	0.012
17.5	0.080	0.039	0.028	0.074	0.041	0.032	0.046	0.108
27.3	0.077	0.067	0.032	0.020	0.047	0.072	0.018	0.045

TABLE 25

SIX-INCH TTF SODIUM DATA FROM SENSOR PAIRS TOGETHER

Thermocouple Type	Temperature (°F)	Mean	Standard Deviation
0.040 in.	500	1.000	0.068
0.040 in.	700	1.042	0.102
0.040 in.	870	1.002	0.050
0.060 in.	500	1.000	0.044
0.060 in.	700	0.958	0.050
0.060 in.	870	0.998	0.032
All 0.040 in.	-	1.007	0.068
All 0.060 in.	-	0.993	0.043
All	-	1.000	0.057

7. Conclusions and Recommendations

Observations of data summaries were presented in paragraphs 5 and 6 of this section. The data presented herein are somewhat preliminary and therefore only limited conclusions have been drawn. Augmentation of these results will be presented in a future report.

8. References

- 1) R. L. Randall, "Transit Time Flowmeter Employing Noise Analysis Techniques, Part 1, Water Loop Tests," AI-AEC-12802 (October 16, 1969).
- 2) R. L. Randall, "Transit Time Flowmeter Employing Noise Analysis Techniques, Part 2, 2-inch Sodium Loop Tests," AI-AEC-12941 (September 30, 1970).

Figure 67. Transit Time Flowmeter Test —
Thermocouple Location Identification

- 3) C. W. Wheelock, "Transit Time Flowmeter; General Plan for Design, Assembly, and Test of a 6-inch Diameter System in Water and in Sodium," TP-707-50-002 (September 10, 1970).
- 4) E. L. Babbe, "Transit Time Flowmeter, Sensor Design and Assembly Description — 6-inch Pipe Size," SDD-707-50-003 (June 3, 1970).
- 5) Staff, "AI Transit Time Flowmeter Test Procedure," TP-04-CE-001 (January 27, 1971).

VII. CHEMICAL AND METALLURGICAL LABORATORY
(LMFBR Program Plan 3-3.1, 5-5.1 through 5-5.7)
D. E. Goggin

A. INTRODUCTION

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

B. CHEMICAL LABORATORY

Trace metal and oxygen analyses of EBR-II sodium received from ANL for the Sample Interchange Program between ANL-Illinois, EBR-II and LMEC has been completed. The techniques used were those described in ANL/ST-6, (to be issued as an RDT standard, RDT F 3-40T) "Interim Methods for the Analysis of Sodium and Cover Gases." The trace metal analytical method, as reported in the last period, was straightforward and adequate. Of the 13 elements analyzed, only a minor problem was encountered in that bismuth was found in the aqueous rather than the alcoholic medium as described in the interim method. Difficulties were encountered in the sample supplied in the extrusion vessel for determination of oxygen in sodium for the range below 5 ppm (Extrusion Method). The sodium was badly contaminated as evidenced by the presence of a large continuous void ($\sim 1/4$ -in. diam by ~ 2 -in. length) and visible oxide on the surface of the sodium sections within the extrusion vessel. Results were high and erratic varying from about 5 to 400 ppm oxygen. These results, as well as those from the trace metals analysis, have been formally reported by letter to ANL with comments and suggestions on the methods for discussion at the next ANL meeting of the National Standards Working Groups scheduled for July 7 and 8, 1971.

An improved method of determining impurities in cover gases was submitted to ANL for consideration and possible inclusion in the next revision of ANL/ST-6. Grab samples taken from varied loop cover gas systems are analyzed with a gas chromatograph using a highly sensitive helium ionization detector. The method permits a large number of test facilities to be monitored by a single gas chromatograph without the necessity to preconcentrate samples as required for less sensitive chromatographs. Long-term successful operation

verifies the general validity of the method; however, problems have been encountered recently with the gas chromatograph. These difficulties are caused by high standing currents and negative, nonreproducible chromatogram peak heights. Corrective action to eliminate the problem by rechecking system integrity and replacement of detectors, traps, and carrier gas is in progress.

Evidence acquired by observation and experimentation in the LMEC Chemistry Laboratory indicates that the high oxygen content problem with MCIS test vessel No. 2 sodium is due to metal particles. Particulate surveillance analysis data obtained from aqueous extracts following oxygen-amalgamation determinations, indicate a general, direct correlation between particulate content and oxygen concentration. One mechanism of particulate interference may be the inability of the mercury to amalgamate all sodium from the particles, which float upon the surface of the mercury, and subsequently lead to erroneous high results. However, it is considered more likely that the particulate is a nucleation site for the oxygen in the sodium system, and this gettering interferes with the ability of a facility cold trap to perform its function.

An analysis technique and test apparatus used to sample and quantitatively determine the presence of sodium vapor in a nitrogen gas stream was set up and operated in coordination with the LMEC Sodium-to-Gas Leak Detection Experiment. In this test, sodium was leaked at a controlled rate from a vessel into a surrounding annulus and swept with nitrogen gas to carrier vapor through variable tubing lengths. The sodium vapor was collected by passage through a liquid trap containing a water-alcohol phenolphthalein solution. The total sodium collected in the solution was quantitatively determined by standard techniques using flame photometry. This technique proved to be the most sensitive of those tested. In addition, a successful in-line checkout of a laboratory pH meter was made. A sodium specific ion electrode monitor was suggested for future leak detection experiments.

Summary data for PSTF analysis programs completed in January 1971, indicated that Fluorolube oil-sodium degradation and reaction products detected in the argon cover gas increased over a period of 5 months from 1 to 63, as measured by relative peak height ratios. The sodium content of Fluorolube oil removed from the lower seal was 446 ppm, which is a significant increase from

~ /ppm in the virgin oil. More recently, samples of a turbine quality hydro-carbon type oil have been analyzed for total sodium content by flame-emission atomic absorption spectrophotometry, following dissolution with methyl isobutyl ketone. Results to date have been somewhat erratic but show a general increasing trend.

Sodium compatibility selection tests with various commercial insulation materials for 1000° to 1350°F service are being conducted. The survey includes rigid-formed types of materials of which four contained hydrous calcium silicate with variable amounts of sodium silicate, asbestos, and diatomaceous earth. Blanket types of insulating "wools," made mainly of alumina silica fiber, are also undergoing compatibility tests. The test technique involves heating about 5 gm of sodium in a metal crucible to approximately 700° to 1000°F in a muffle furnace under argon pruge. The molten sodium is poured in air onto the sample insulation which had been dried a minimum of 4 hr at 250°F. At the test temperature, the molten sodium has pre-ignited, a condition thus varied from loop system environment where the air supply is limited in the insulation-to-pipe region. A report which describes the tests and discesses of the results is planned for the next report period.

Laboratory capability has been considerably broadened during this report period and will continue to expand during the next semiannual period. Recently ordered equipment will be acquired and installed. The purpose of the laboratory capability expansion is twofold: (1) accommodate ANL/ST-6 methods for the characterization of sodium and cover gases; (2) support facility construction, modification, and surveillance efforts attendant to various LMEC programs. Principal capability improvements include: (1) on-stream operation of a Perkin-Elmer 403 Atomic Absorption Spectrophotometer for trace metal analysis; (2) acquestion of an X-ray focusing monochromator for the X-ray diffractometer to increase sensitivity; (3) replacement of an obsolete photo-processor used with the emission spectrograph; and (4) installation of a Leco Low Carbon Analyzer to complement the present X-ray fluorescence (XRF) capability and allow a complete material identification/certification in-house of austenitic stainless steels used in various facilities at LMEC. Equipment is on order to adapt the methods outlined in ANL/ST-6 for determining the hydrogen, carbon, and oxygen (by the vanadium wire equilibration method) content of sodium.

C. METALLURGICAL LABORATORY

A defective Wm. Powell 1-1/2-in. gate valve body, Type CF8M stainless steel, was sectioned and metallurgically examined in the laboratory. Of major interest were three defective areas defined by previous nondestructive examinations. Sections of these areas were removed and a series of transverse cuts were made through the defective zones to determine the character and depth of the defects. Suitably prepared macrosections were examined and photographed. Defect depths in the form of single fractures of up to 90% of the valve body wall thickness were measured. One area contained discontinuous irregular sub-surface cracks where the wall thickness was degraded by approximately 50%.

During valve disassembly, a group of metallic spheres was found lightly adhering to the inside surface of the lower valve body. These spheres were subsequently identified by XRF semiquantitative analysis as a nickel-base alloy containing approximately 15 wt % Cr and less than 10 wt % Fe, and were ascertained to be a brazing alloy used by the manufacturer.

Specimens of the initial procurement of stainless steel pipe purchased from U. S. Steel to RDT Standard M 3-3T were submitted for metallographic examination. This pipe is intended for use in various LMEC facilities construction. Three suspect areas have been examined revealing inside diameter laps or seams (Figure 68) of approximately 0.005, 0.007, and 0.015 in. This exceeds the allowable wall defect of $3 \pm 0.5\%$ specified in RDT M 3-3T. In addition, a length of 6-in.-diam schedule 160 seamless pipe per ASTM A376, Type 304 (0.04 C min.) was evaluated to upgrade to RDT M 3-3T requirements and use in PBTF spare pump modification. Chemical analysis has satisfactorily verified material composition. A hardness traverse made on a cross section from OD to ID confirmed the pipe to be in the annealed condition with an acceptable hardness range. Metallographic examination revealed a normal microstructure. A report summarizing the defect indications found in Type 304 SS pipe purchased to the RDT standards was transmitted on June 18, 1971.

Seven sections from the failed LCTL core tank (T-1) lid were received for metallographic examination. The locations of these samples are shown in Figure 69. Metallographic examination of two cracks in Sample C disclosed two main intergranular cracks with numerous smaller secondary intergranular

← a. Defect 0.005 in.

b. Defect 0.007 in. →

← c. Defect 0.015 in.

LMEC-P37001

Figure 68. U.S. Steel Schedule 40 SS Pipe,
Heat No. 9T-2797 ID Defects (Mag 100X)

LMEC-37002

Figure 69. Top View of LCTL Core Tank Lid Dished Head, Showing Crack Distribution Before Sodium Cleaning and Sample Locations

fissures originating from the main cracks. In some areas adjacent to the main crack, decohesion of grains due to intergranular attack was also evident. However, transgranular cracking with its familiar branching, typical of stress corrosion, was also observed in several localized areas. The microstructures of the material in Sample C exhibited carbide precipitation in grain boundaries, in twin boundaries, and in deformation lines within the grains. Further, the material in Sample C appeared to be cold-worked, with much heavier indications toward the outer surfaces. Microstructural examination of Samples B and E also disclosed similar types of crack morphologies (i. e., predominant intergranular fracture with some branching transgranular mode in localized areas).

Metallographic examination of Sample D revealed a large amount of branching transgranular fissures from what appeared to be the main intergranular crack. In addition, the material in Sample D exhibited little or no carbide precipitation in grain boundaries or twin boundaries. The material also displayed cold work indications on the outer surface areas.

For Samples W-1, W-2, and W-3, the material exhibited little or no sensitization. Some cold work indications were observed in localized areas. The fracture mode was entirely transgranular, with cracks emanating from the outer surface in the crevice.

Seven samples were received for chemical analyses; the type and location are shown in Table 26. Qualitative chemical analysis of two samples of deposits scraped from the lid at locations shown in Figure 69 showed the major constituents to be sodium and potassium. As shown in Table 26, appreciable amounts of chloride, fluoride, and sulfur were found in the insulation (vermiculite), even in its new unused form.

X-ray diffraction analysis of the corroded fracture surfaces revealed many diffraction peaks. NaFeO_2 and Na_2CO_3 were found to be present. However, most of the other peaks were not identifiable.

Specimens of plate material ordered to RDT M 5-1T were examined metallographically to further characterize the material. The grain size and microhardness were measured, and the inclusion content was determined in accordance with ASTM E45-63. Results of the metallographic examination were sent to ORNL for their information.

TABLE 26
RESULTS OF CHEMICAL ANALYSES

Sample Number	Sample Location	Approx. Concentration (ppm)			Identification Method	Remarks
		Cl	F	S		
D-1	Crack No. 1 in D	9,900	100 & 700	-	Specific ion electrode	Na-residue scraped from dome interior before cleaning
				†	XRF	
D-3	Crack No. 3 in D	10,200	76	-	Specific ion electrode	Na-residue scraped from dome interior before cleaning
				†	XRF	
Crevice No. 1	Residue from crevice in Sample W-1	2,300*	75*	-	Specific ion electrode	Residue and vermiculite from lid crevice
				†	XRF	
LCTL-1	Vermiculite from LCTL lid	150	-	60	XRF	Vermiculite from LCTL core tank lid
		44*	6*	-	Specific ion electrode	
				$\text{SO}_4^{2-} \approx 165$	Turbidimetry	
Unused-2	Brand new vermiculite	75	-	50	XRF	New vermiculite from Rocketdyne
		40*	6*	-	Specific ion electrode	
				$\text{SO}_4^{2-} = <100$	Turbidimetry	
Heck Check Solution	Leak detection solution	6	<1	None	Specific ion electrode	Solution used to confirm leakage in cracks
Unichem Traceleak	Leak detection solution	500	20	-	Specific ion electrode	Solution used initially to confirm leakage on four major cracks. Its use was discontinued.
				>1000	XRF	

*Leachable anions.

†Uncertain of its presence because of the possible interference from Mo impurity in the sample.

VIII. 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 and to modify the LCTL to permit operation at LMFBR temperature and pressure conditions. Since the FFTF Flow Duct/Receptacle Erosion-Flow Test (T-1D) was completed in December 1970, effort this reporting period has been concentrated on maintenance of the facility and examinations of piping and components.

B. LCTL MAINTENANCE AND SYSTEMS EXAMINATIONS

During a scheduled inspection of the LCTL core tank (T-1) lid on April 13, 1971, numerous radial cracks and corrosion pits were discovered in the dished head. These cracks did not cause damage to the facility or injury to operating personnel. The incident was reported in IPR-LCTL-70 and a detailed failure analysis of the cracked lid was performed.

The LCTL core tank lid is the upper closure of the vertically oriented sodium core tank. Constructed of Type 304 stainless steel, the tank is 8 ft in diameter, 31 ft long, and has a wall thickness of 5/8 in.

As shown in Figure 70, the semi-ellipsoidal dished head portion of the core tank top closure is recessed below the upper flange (connection between the lid and tank shell). The dished head and cylindrical skirt form a reservoir, which was filled with vermiculite insulation (~2 ft deep).

The failure analysis performed on the lid disclosed a total of 29 cracks on the external surface of the dished head. Twenty-four of these cracks were also visible on the interior surface, indicating total penetration. A number of the most prominent failures are clearly evident in Figures 71 and 72.

Based on the results of metallographic, chemical, thermal, and stress analyses on the cracked core tank lid, it can be concluded that:

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Figure 70. LCTL Core Tank (T-1) Lid with Cover Plate

LMEC-P32037A

Figure 71. External View of LCTL Core Tank (T-1)
Lid Before Sodium Cleaning

LMEC-71-7

Figure 72. Interior View of Dished Head, Showing Annular Ground Areas and Distribution of Cracks

- 1) The cause of failure of the lid is attributed to stress corrosion of Type 304 SS by corrosives and/or impurities leached from the vermiculite insulation by rain and/or moisture collected in the recessed space below the rectangular opening of the lid cover plate.
- 2) The identified corrosives from the insulation are chloride ions, fluoride ions, and sulfate ions. Because unprotected insulation could act as a natural collector for atmospheric impurities, a supply of these elements could also have come from the atmosphere at LMEC due to its close proximity to a rocket engine testing site.
- 3) For sensitized material in the dome, stress corrosion cracks propagated primarily along intergranular paths. For unsensitized dome material in the crevice area, stress corrosion cracks propagated transgranularly.
- 4) Crack initiation and propagation were primarily governed by:
 - a) Residual stresses in the dished head due to fabrication
 - b) Internal pressure stresses
 - c) System stresses due to thermal expansion
 - d) Stresses produced by the wedging action of corrosion products.
- 5) All of the cracks initiated from the external surface of the dome.
- 6) The path of crack propagation was governed by system geometry and stress pattern in the dished head.

A thorough inspection of the LCTL core tank was also performed to determine material integrity. No cracks or corrosion indications were disclosed and it has been concluded that the core tank material is in a satisfactory condition.

Examinations of the LCTL supply and drain tanks (T-2 and T-3) are scheduled during July 1971.

A topical report, "Sodium Piping and Component Failures at the Large Components Test Loop (LCTL)," LMEC-Memo-71-5, was published July 15, 1971. This report presents descriptions and failure analysis results of material failures at the LCTL.

IX. CREEP RATCHETTING LABORATORY TESTS
(LMFBR Program Plan 3-8.2, 3-8.3, 3-8.4)
J. J. Auleta

A. INTRODUCTION

LMEC has been conducting a continuing study of the performance limitations imposed by structural design criteria of LMFBR components. Previous correspondence with AEC-RDT has discussed the limitations imposed by fatigue of piping components and an initial evaluation of the potential limits of creep ratchetting on component design. The limits imposed by the creep ratchetting appeared to be the most restrictive on LMFBR capabilities.

Ratchetting deformation is induced in metal components by the synergistic effect of a continuous, uniform-in-space, mechanically induced stress, which acts simultaneously with a cyclic, nonuniform-in-space, strain-induced stress.

A literature search revealed the following study of this problem. Ratchetting has been investigated analytically for low-temperature conditions by Miller,^{(1)*} Gatewood,⁽²⁾ Bree,⁽³⁾ Burgreen,⁽⁴⁾ and others.

The possibility of ratchetting deformation being further enhanced by creep deformation (creep ratchetting) at high metal temperatures has been investigated by Bree,⁽⁵⁾ and Leckie and Ponter.⁽⁶⁾ Results of experiments to confirm analytical predictions at low temperatures have been presented by Gatewood et al.,⁽⁷⁾ and Edmunds and Beer.⁽⁸⁾ Results of tests at temperatures in the metal creep range have been reported by Leckie.⁽⁹⁾

Service experience with pressure vessels subjected to ratchetting conditions has been reported by Weil and Rapasky.⁽¹⁰⁾

Because of the lack of easily interpreted experimental data, the problem of creep ratchetting has not received adequate consideration in conventional design procedures and criteria. In August 1970 a proposal was presented to AEC-RDT for a limited test program to be undertaken to obtain experimental verification of the magnitude and effect of creep ratchetting in high-temperature, sodium-cooled reactor design. An additional purpose was to obtain quantitative data to provide a basis for evaluation of analytical methods of predicting the magnitude of creep

*References are listed at the end of this section.

ratchetting deformation. A principal objective was to check the accuracy of analytical predictions of deformation per cycle using curves from Bree.⁽⁵⁾ Bree evaluated the magnitude of creep ratchetting for cases of combined, continuous, uniform mechanical stress, and cyclic thermal stress varying linearly through the thickness such as is induced by steady-state heat conduction through a slab. Graphical results were presented in nondimensionalized form for only the case of a power law for secondary creep with the exponent $n = 6$. The material for the test program has a value of $n = 9.4$ and the effect of this variable is not sufficiently understood to extrapolate Bree's results. Because of this limitation in the applicability of the analytical results reported by Bree,⁽⁵⁾ it was necessary to also develop a simplified analytical method with sufficient flexibility in application to help guide an experimental program.

Testing was initiated in January 1971. This report includes the results of testing since then. It also includes in Appendixes A and B the results of allied efforts on analysis and instrument development directly related to the test effort, though separately funded. The development of the analytical method was reported separately⁽¹¹⁾ and will soon be available in the open literature.

B. A MODEL FOR DESCRIPTION AND ANALYSIS OF RATCHETTING

The analytical method presented herein considers the problem in a manner similar to that of Burgreen⁽⁴⁾ and Bree,⁽⁵⁾ in that a residual stress pattern induced by the cyclic stress is determined for a homogeneous, elastic, perfectly plastic solid. This method then uses a two-layer solid derived from the previous result to estimate the creep effect. Provisions can be made to allow for step changes in time in the mechanically induced, uniform-in-space stress. It differs from Bree⁽⁵⁾ also in that it considers that the cyclic thermal stress has a quadratic parabolic distribution in space. The results are presented for use with a simple computer program for use on a time-sharing terminal, rather than attempt graphical results including a significant range of all parameters. Graphs are presented which are used primarily to provide input to the computer program.

1. Assumptions

The analytical method is based on the following assumptions:

- 1) The thermal stress distribution at the time of the peak stress can be represented with sufficient accuracy by a quadratic parabola through

the thickness and is uniform in this respect through the length and breadth of the wall. The full ΔT of the parabola is set equal to the peak ΔT during the transient. Figure 73, taken from Minami and Roberts,⁽¹²⁾ presents the temperature distribution in space at various times during a thermal transient with the peak stress at Time (4).

- 2) The mechanically induced stress has constant magnitude through the thickness and is uniform in this respect through the length and breadth of the wall. This stress is subject to step changes of magnitude in time.
- 3) The cross section is restrained from bending deformation so that plane sections remain plane and parallel. The total elastic stress distribution is then reasonably depicted by Figure 74a.
- 4) The time-independent properties of the material are sufficiently well expressed by the properties of an elastic, perfectly plastic solid. To further refine the analysis, the yield strength utilized can be adjusted to include the effect of work hardening, kinematic or isotropic, though such changes are not inherent to the method presented herein.
- 5) The time-dependent properties of the material are sufficiently well expressed by a power law for creep rate:

$$\dot{\epsilon} = \left(\frac{\sigma}{\sigma_{eo}} \right)^m ;$$

and a power law for time-to-rupture:

$$t_r = \left(\frac{\sigma}{\sigma_{ro}} \right)^{-n} .$$

The effective creep rate so expressed may be taken as the average rate of time-dependent deformation through the range from zero creep strain to that creep strain which equals some arbitrarily established deformation limit. This average rate is then assumed to adequately allow for the primary creep strain and also to allow for extrapolation of analytical predictions. For biaxial states of stress, the creep rate in one direction is assumed to be 1/2 the effective rate.

- 6) The structural properties of the actual solid are assumed to be homogeneous for the plastic time-independent behavior and for establishing residual stresses.

Figure 73. Calculated Transient Distribution Temperature

Figure 74. Stress Distribution in the Homogeneous and Two-Layer Solid

- 7) The structural properties of the homogeneous solid with its residual stresses are assumed to be adequately represented by a two-layer solid. The relative thickness of the two layers is established by equilibrium requirements so that significant values of peak stress from the inner and outer regions of the homogeneous solid are maintained in the two-layer solid (see Figure 74b).
- 8) The relative thickness of the two layers is assumed to remain unchanged from the first cycle through subsequent cycles. This makes no allowance for the actual residual stress remaining after the hold time of any cycle.
- 9) Rupture life usage (rupture damage) is assumed to occur only in the presence of tensile stress, σ_a . The effect of this time-varying tensile stress on rupture life usage is adequately represented by

$$u_r = \int_{t=0}^{t_i} \frac{dt}{t_r(\sigma_a)} \quad .$$

This is assumed to adequately allow for the hold time effect in the creep-fatigue interaction as shown by Campbell.⁽¹³⁾

- 10) It is assumed that no creep deformation occurs during the time period when the thermal transient reaches its peak and subsides and the operating (creep) temperatures are restored. This may also be assumed to be true during the periods when the operating pressure (mechanical stress) is removed and restored.

2. Determination of Residual Stress

Stresses are nondimensionalized by using any compatible system of measurements as follows:

$$e_y = \sigma_y / E$$

$$\beta = P_m / \sigma_y$$

$$\gamma = \frac{\alpha \Delta T}{e_y} = \frac{E \alpha \Delta T}{\sigma_y} \quad ,$$

where

P_m = mechanically induced uniform stress

$\sigma_a(t)$ = time-dependent stress in outer wall

$\sigma_b(t)$ = time-dependent stress in inner wall

σ_y = yield stress for material

E = Young's Modulus (taken as $E/(1 - \nu)$ for biaxial cases)

e_y = total elastic strain associated with the yield stress

α = coefficient of linear thermal expansion

ΔT = total difference in temperature through the wall

t = time

Φ = ratio of thickness of inner wall layer to outer wall layer

Ψ = measure of residual stress at outer layer such that $\sigma_a = P_m + \Psi\sigma_y$.

Then in the outer layer $0 \leq z < h_1$,

$$\sigma_a = P_m + \Psi\sigma_y = (1 + \Phi)P_m + \Phi\sigma_b \quad ,$$

and in the inner layer $h_1 < z \leq h_2$,

$$\sigma_b = \frac{1}{\Phi} \left[\sigma_a - (1 + \Phi) P_m \right] \quad .$$

The required inelastic analysis changes with the ratios of applied stress β and γ . Figure 75 shows various regimes of inelastic behavior for values of β and γ . This is taken from Burgreen⁽⁴⁾ and is similar to Bree.⁽⁵⁾ In the regime E, the maximum stress never exceeds σ_y and there is no nonuniform residual stress.

As shown in Figure 76, tensile yielding starting at depth z_2 occurs on the inner wall in regimes S_1 , S_2 , R_1 , and R_2 at the peak of the transient stress. The outer wall does not yield at this time. In S_1 no yielding occurs at the inner wall in establishing the residual stress σ_a or σ_b . However, in R_1 tensile yielding occurs at the outer wall and projects into the region where tensile yielding occurred on the inner wall during the peak of the transient. This indicates that time-independent ratchetting, in addition to creep ratchetting, occurs in regime R_1 , while only creep ratchetting can occur in regime S_1 .

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Figure 75. Regimes of Ratchetting Growth per Thermal Cycle; Parabolic Temperature Distribution

Figure 76. Stress and Strain in Regimes S_1 and R_1

As shown in Figure 77, compression yielding occurs at the inner wall to the depth z_3 in establishing the residual stress pattern σ_a and σ_b in regimes S_2 and R_2 . In S_2 the outer wall does not yield. In regime R_2 the outer wall yields in tension in establishing the residual stress and extends into the region where tensile yielding occurred at the peak stress. This indicates that time-independent ratchetting, in addition to creep ratchetting, occurs in regime R_2 , while only creep ratchetting can occur in S_2 .

As shown in Figure 78 for the regimes P_1 , P_2 , and R_3 , the outer surface yields in compression to a depth $z = z_1$ at the time of the peak transient stress. Meanwhile, the inner surface also yields in tension, out to a depth $z = z_2$. Subsequently, in P_1 , P_2 , and R_3 , yielding occurs at the inner wall out to

Figure 77. Stress and Strain in Regimes S_2 and R_2

LMEC-45035

Figure 78. Stress and Strain in Regimes P_1 , P_2 , and R_3

the depth z_3 as the transient subsides in establishing the residual stress. No tensile yielding occurs in the outer wall in regime P_1 , whereas in P_2 tensile yielding can occur but only out to the point z_1 . If tensile yielding occurs beyond z_1 , it immediately projects over into the range beyond z_2 , where tensile yielding occurred during the peak transient stresses. Under such conditions the stresses are in regime R_3 and both time-independent and creep ratchetting can occur, whereas in regimes P_1 and P_2 only creep ratchetting can occur.

The stress $\sigma_a = P_m + \Psi \sigma_{yp}$ is established equal to the stress in the region $z_1 < z < z_2$ for the two-layer solids. The total residual stress pattern is used in establishing equilibrium and the value of σ_a . However, in creep analysis of the two-layer solid, the increased stress in the region $0 < z < z_1$ is ignored.

The relatively insignificant effect of the stresses in region $0 < z < z_1$ indicated that reasonable results would be obtained if P_1 relations were used in P_2 , and this has been done with no further proof. P_1 extends through the value $\gamma = 7.0$ so that values of Φ and Ψ are accurate through this range.

The results of solving for a range of values of Ψ and Φ are presented graphically in Figures 79 and 80 as values of Ψ and Φ for values of β and γ .

Figure 79. Ψ - β Relation for Values of γ

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Figure 80. $\Phi - \beta$ Relation for Values of γ

Figure 81. Stress and Strain Change During Creep Ratchetting Hold Time

3. Effect of Hold Time at Creep

Figure 81 (a and b) depicts the change of stress and strain which occurs during an increment of time in the creep ratchetting process. At the end of the transient subsidence and at time zero in the hold period — Time (9) of Figure 73 — the stress in the outer layer is $\sigma_{a0} = P_m + \psi\sigma_y$. The stress in the inner layer is σ_b . If pure relaxation were to occur in both layers at the rate determined by the secondary creep rates $\dot{\epsilon}_a$ and $\dot{\epsilon}_b$ with zero net strain during a subsequent time interval ΔT , then the stress change in the two layers $\Delta\sigma_a$ and $\Delta\sigma_b$ would be as shown on Figure 81a. Such a stress change usually results in the equilibrium condition not being satisfied. When equilibrium is not satisfied, an increment of mechanical strain Δe_m is induced which modifies the stresses in both layers so that equilibrium is maintained. Figure 81 shows Δe_m being introduced as a positive increment so that σ_a is increased and the absolute value of σ_b is decreased. In actual cases Δe_m may be either positive or negative and may change from negative to positive during the hold period. Then, during the time increment Δt , the total of creep ratchetting strain and conventional creep deformation is expressed by

$$\Delta e_r = \Delta e_m - \frac{\Delta\sigma_a}{E}, \quad \dots(1)$$

and the rupture damage life usage fraction is expressed by

$$\Delta U_r = \frac{\Delta t}{t_r(\sigma_a)} \quad \dots(2)$$

The amount of time-independent ratchet strain per cycle e_g is also shown in Figure 75 for regimes R_1 , R_2 , and R_3 where this phenomenon occurs. This is taken from Burgreen.⁽⁴⁾ This value of e_g must be added to time-dependent ratchet strain per cycle e_r to obtain the total ratchet deformation e_{rt} during a cycle with conditions in these regimes.

The stress-strain relation for the outer layer during a creep ratchetting cycle representative of regime S_2 is shown on Figure 82. At Time (0), the strain e_1 is the mechanical strain induced by the uniform mechanical stress. At Time (4), the transient stress is at its peak and results in compressive stress about equal to the magnitude of the yield stress, but in this case no compressive yielding occurs. At Times (6) and (9), the transient stresses have subsided at a stress about equal to the tensile yield stress but no tensile plastic strain occurs. The total strain is $e_1 + e_2$ consistent with the strain needed to maintain the residual stress in the outer layer. This would indicate a substantial step increase in width and breadth of a plate, or length of a strip, as a result of the transient stress. The temperature is restored between Times (6) and (9). During the hold time, the tensile stress relaxes and the mechanical strain starts to decrease, then finally assumes a net positive value at Time (10). The total creep ratchet strain in the first cycle is then $e_2 + e_3$. This exceeds the amount of creep ratchet strain which occurs in subsequent cycles e_4 by the amount e_5 which represents the difference between the stress in the outer layer at the start of cycle 1 and at the start of cycle 2. The computer program and analysis are formulated to predict that value shown for cycles 2 and 3 as e_4 as the value of the creep ratchet strain e_r .

The stress-strain relation for the inner layer for the same cycle is shown in Figure 83. Note that tensile plastic strain occurs at the peak transient stress and compressive plastic strain occurs as the thermal transient subsides, as is the case in regime S_2 . The starting strain e_1 , the prompt increase in strain due to the transient e_2 , the mechanical strain e_3 , and the total creep ratchet strain in subsequent cycles e_4 are all identical to those values for the outer layer.

The relations of the outer layer may be obtained for the range of β and γ covered by Figures 79 and 80 by use of the time-share computer program in Appendix A. The equations solved by this program are based on maintaining equilibrium of forces and compatibility of strain in the two layers:

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Figure 82. Stress-Strain Relations of Outer Layer

LMEC-45040

Figure 83. Stress-Strain Relations of Inner Layer

Equilibrium of Forces yields

$$\sigma_a = (1 + \Phi) P_m - \Phi \sigma_b \quad \dots (3)$$

$$\dot{\sigma}_a = -\Phi \dot{\sigma}_b \quad \dots (4)$$

Compatibility of Strain yields

$$\dot{\epsilon}_a = \left(\frac{\sigma_a}{\sigma_{eo}} \right)^m + \frac{\dot{\sigma}_a}{E} = \dot{\epsilon}_b \quad \dots (5)$$

$$\dot{\epsilon}_b = \left(\frac{\sigma_b}{\sigma_{eo}} \right)^m + \frac{\dot{\sigma}_b}{E} \quad \dots (6)$$

These allow the expression of rate of change of stress and strain in the outer layer as

$$\dot{\sigma}_a = -\frac{\Phi}{1 + \Phi} \cdot \frac{E}{\sigma_{eo}^m} \left[\sigma_a^m + \left(\frac{\sigma_a}{\Phi} - \frac{1 + \Phi}{\Phi} P_m \right)^m \right] \quad \dots (7)$$

$$\dot{\epsilon}_a = \left(\frac{1}{\sigma_{eo}} \right)^m \left(\frac{\Phi}{1 + \Phi} \right) \left[\frac{\sigma_a^m}{\Phi} - \left(\frac{\sigma_a}{\Phi} - \frac{1 + \Phi}{\Phi} P_m \right)^m \right] \quad \dots (8)$$

The creep ratchet strain rate is then

$$\dot{\epsilon}_r = \dot{\epsilon}_a - \frac{\dot{\sigma}_a}{E} \quad \dots (9)$$

The rupture life usage rate is

$$\dot{u}_r = \frac{1}{t_r(\sigma_a)} = \left(\frac{\sigma_a}{\sigma_{ro}} \right)^{+n} \quad \dots (10)$$

Input to this program involves material properties E , σ_y , S_{eo} , and $XE = m$ from the creep relation

$$\dot{\epsilon} = (\sigma / S_{eo})^{XE} \quad ,$$

and S_{ro} and $XR = n$ from the rupture time relation

$$t_r = (\sigma / S_{ro})^{-XR} \quad .$$

The value of mean stress P_m and values of Ψ and Φ obtained from Figures 79 and 80 are also part of the input. The desired number of lines of output is an additional input and results in printing out the increments and totals of stress, strain, and time for the outer layer at a number of points in time equal to the number of lines output requested. The actual number of time increments in the calculation may be much larger than the number in the output. Actual time increments used in the calculation are calculated internally by setting the Δt so that the rate of stress change $\dot{\sigma}_a$ induces a change of stress $\Delta \sigma_a = 0.02 \sigma_a$. The most simple of forward integration procedures are used to calculate the total ratchet strain and rupture life usage.

C. EFFECT OF CHANGING-PRESSURE STRESS

The effect of reducing and then restoring the pressure after the transient has subsided is shown in Figure 84. If the compressive stress on the inner layer were required to exceed the compressive yield strength when the pressure is reduced, as would always be the case in regime S_2 , then an irreversible strain change takes place which results in obtaining a reduced stress in the outer layer after the cycle is complete, as shown by σ_{a2} . To calculate the subsequent amount of creep ratchetting, the value of $\Psi = (\sigma_{a2} - P_m) / \sigma_y$ would be the input to the computer program along with the initial value of Φ .

LMEC-45041

Figure 84. The Effect of Pressure Changes

D. CHECK OF APPROXIMATE ANALYTICAL RESULTS

The problem shown in Figure 73 was solved by Minami⁽¹²⁾ using the more sophisticated computer programs. The results of predicting stress distributions agreed within 10% in magnitude, which is not unreasonable, since the problem falls in the regime P_2 where most error in the two-layer theory is expected. The results of calculations of ratchetting deformation per cycle agree very well: 59 microinches per cycle by Minami⁽¹²⁾ and 60 microinches per cycle using the approximate method.

E. TEST EQUIPMENT AND PROCEDURES

The test equipment was developed by rebuilding equipment which was initially used for testing high-temperature strain gages on beams subjected to two-point loading. Figure 85 shows this equipment before it was modified. Three such furnaces were modified but only two have been used to date. An outline of the equipment as modified is shown on Figure 86. For the creep ratchetting tests, two mandrels of circular curvature were mounted on the moving frame. One of these, generally the mandrel with the smaller radius of curvature, is used as the bend mandrel. The other mandrel with a restricted travel is used as the hold mandrel. A hole was cut in the face of the furnace and a loading rod was inserted for applying the axial stress to the specimen through a pin. The loading rod is placed in tension by a calibrated compression spring. A dial indicator is mounted at the end of the loading rod and is used to indicate total axial extension of the specimen. A pin mounting at the back of the furnace holds the other end of the specimen. Another dial indicator is mounted at the side of the furnace and is used to indicate the transverse motion of the mandrels and specimen as the specimen is bent over the bend mandrel and then returned to the hold mandrel.

Specimens are nominally 1/8-in. by 1/2-in. strips, 12 in. in length with pin adapters welded on the ends. Gage marks are placed on the side of the specimen at 0.10-in. intervals over the center 2 in. of the length. Additional gage marks at 2.0-in. intervals are made outside this center interval. The location of each mark is read with a telescopic micrometer at room temperature before testing the specimen and again at the conclusion of the test to determine the magnitude and distribution of axial strain. Locations are read six times under both conditions to obtain reasonable estimates of the accuracy of the results. The root mean square value of the variation in distances is approximately 200 microinches.

7698-1817

Figure 85. Creep Ratchetting Laboratory Test Equipment
Prior to Modification

Figure 86. Creep Ratchetting Laboratory Test Device

The maximum axial load on a specimen is 850 lb. The transverse component of this load is on the order of 80 lb, so friction does not significantly affect the axial load in the specimen.

The temperature is controlled automatically from a furnace-mounted thermocouple. Temperature is observed from a thermocouple mounted on one mandrel. The temperature is held constant throughout the test.

The test is started by setting the hold mandrel against the specimen and leaving it at test temperature overnight to establish the curvature of the hold mandrel as the base configuration of the specimen. Then each morning the specimen is bent over the bend mandrel and promptly returned to the hold mandrel and maintained at that position for 24 hr for this group of tests.

F. TEST RESULTS

Table 27 shows the test conditions and the results of six tests at 1200°F on specimens of Type 304H stainless steel. Three tensile tests and six creep tests were performed at 1200°F to determine the properties of the material under monotonic loading. The results allow expression of the 1200°F properties as follows:

$$\sigma_{y(0.2\%)} = 23,500 \text{ psi, } 1200^\circ\text{F}$$

$$\dot{\epsilon} = (\sigma/50,500)^{9.4} \text{ in./in./hr to 1\% creep strain}$$

TABLE 27
CREEP RATCHET TEST RESULTS

Item	Specimen Number							
	1	2	3	4	5	6	7	8
	1200°F						1100°F	
<u>Test Conditions:</u>								
1. Specimen thickness (in.)	0.1155	0.1135	0.1155	0.1165	0.1165	0.1165	0.1125	0.1140
2. Specimen width (in.)	0.520	0.520	0.521	0.518	0.519	0.5185	0.5085	0.5080
3. Hold mandrel radius (in.)	80	80	80	80	80	40	80	80
4. Bend mandrel radius (in.)	40	20	80	80	40	20	40	20
5. Bend strain/ $e_y = \frac{1\alpha\Delta T}{2e_y}$	1.92	3.16	1.29	1.30	1.94	3.88	-	3.17
6. Axial load (lb)	712	443	830	712	716	367	710	444
7. Axial stress/ σ_y	0.504	0.319	0.586	0.503	0.504	0.258	0.505	0.318
8. No. of cycles on test	10	15	12	15	11	16	7	7
9. No. of hours under load at test temperature	265	380	310	380	290	400	2128	2128
<u>Test Results:</u>								
10. Total strain at center inch (in./in.)	0.0074	0.0049	0.0038	0.0029	0.0063	0.0050	0.0045	0.0042
11. Average strain/cycle:								
a. Full test	0.00074	0.00033	0.00032	0.00019	0.00057	0.00031	0.00064	0.00060
b. First 7 cycles	0.00085	0.00054	0.00040	0.00030	0.00068	0.00046	0.00064	0.00060
<u>Predictions:</u>								
12. Creep at axial stress only	0.00032	0.0000065	0.00155	0.00046	0.00035	0.00008	*	†
13. Ratchet strain/cycle:								
a. Based on $\sigma_{y(0.2\%)}$	0.00140	0.00098	0.00112	0.00052	0.00140	0.00090	*	†
b. Using Bree's curves	0.00112	0.00104	0.00058	0.00036	0.00112	0.00084	*	†
14. Rupture life usage test material data	0.042	0.032	0.036	0.020	0.042	0.029	*	†
15. Allowable fatigue cycle	3,000	400	40,000	40,000	2,800	200	*	†
16. Time-independent ratchet deformation/cycle at code yield	0.00236	0.00234	0.00216	0.00131	0.00251	0.00225	*	†
17. Approximate total ratchet deformation/cycle at code yield	0.0033	0.0030	0.0029	0.0017	0.0035	0.0029	*	†
<u>Comparison of Test Results and Predictions:</u>								
18. Total deformation to predicted creep	23	750	2.5	6.3	18	6,250	*	†
19. Total deformation to predicted ratchet full life:								
a. Based on item 13a	0.52	0.34	0.29	0.37	0.41	0.35	*	†
b. Based on item 13b	0.66	0.32	0.55	0.53	0.51	0.37	*	†

*Estimated as approximately the same as Tests 1 and 5.

†Estimated as approximately the same as Test 2.

$$t_r = (\sigma/37,400)^{-8.4} \text{ hr}$$

$$E = 20.9 \times 10^6 \text{ psi (Code value)}$$

$$\text{Linear hardening rate} = 0.0238 \times E \text{ psi}$$

$$\alpha = 11.2 \times 10^{-6} \text{ in./in./}^\circ\text{F (for equivalent temperature difference).}$$

The predictions of ratchet strain per cycle given under 13a of Table 27 are based on the above properties using the computer program of Appendix A. The predictions under 13b result from using the curves of Bree⁽⁵⁾ and material properties as above except that the creep strain rate at the yield stress is 4.5×10^{-4} in./in./hr. This value of creep rate and the exponent of stress $n = 6$ results in agreement with the experimentally determined value at the stress of 20,000 psi.

Examination of the specimens after the test showed no evidence of surface cracking from creep and stress rupture. Metallographic examination and micro-hardness tests indicate that a small amount of hardening occurred in all the specimens including a nonstressed control specimen. No significant hardening variation could be detected which might be due to corresponding variation in cyclic strain through the specimen.

Also shown on Table 27 are the results of tests of specimens 7 and 8 at 1100°F. No tensile or creep tests were performed at 1100°F. It is assumed that the tensile properties are approximately the same as at 1200°F and the creep properties vary by a time factor of between 15 and 40. The 1100°F tests were thus performed with hold times of 13 or 14 days, as a matter of operational convenience and decreasing the time on test.

Figures 87 and 88 show the axial extension-time relation from tests of specimens 1 and 2. Data from tests 7 and 8, which were performed under identical conditions (except for time-at-temperature), are also shown on Figures 87 and 88 with the test time scaled by the hold time in days.

G. DISCUSSION

Figures 87 and 88 also show the predicted rate of creep ratchetting deformation for specimens 1 and 2 based on the tensile and creep data presented. The prediction of strain in 13a of Table 27 was made using the approximate method presented here, even though the cyclic strain through the thickness varies

LMEC-45043

Figure 87. Strain-Time Relation for Specimen 1

LMEC-45044

Figure 88. Strain-Time Relation for Specimen 2

linearly rather than parabolically. The curves of Bree⁽⁵⁾ were used to determine the predictions of 13b in Table 27 even though the creep expression admits large errors. The substitution of this analysis was considered to induce little additional error in predictions because four of the six tests were performed on the boundary between the S and R (or P and R) regions where the outer surface is stressed in tension to $+\sigma_y$ to the depth z_2 , and the values of β and γ are the same for both cases on this boundary; thus, values of Ψ and Φ would be the same for either model. Some of the difference between 13a and 13b in Table 27 can result from the difficulty of interpolating in Bree's curves.

From Figures 87 and 88 it can be seen that the results of the analysis are very dependent upon the value of yield strength used and it appears that greater accuracy in the prediction for strain at the final cycles can be achieved by using the stress associated with the peak strain expected in the final cycles. The prediction is only expected to be accurate at one point in time. The predictions were made for the center portion of the specimen and are modified by multiplying by the experimentally determined effective length so they can be shown to the same scale as the total axial extension. The variation due to yield strength was calculated using the method presented here. Bree's method is expected to yield similar and even more accurate results.

The observed strain-time relation is generally the same as that predicted. At the end of the first bend cycle, the extra-large residual stress causes a prompt increase in length similar to e_2 of Figures 82 and 83. Some positive increase in length occurs similar to e_3 during the hold period, and then a prompt increase in length occurs after the next bend cycle similar to $e_4 - e_3 < e_2$ of Figure 82. This first cycle behavior has been allowed for in the predictions, as may be seen on Figures 87 and 88.

It may be observed from items 19a and b of Table 27 that the predicted creep ratchetting deformation is between 2 and 3 times the average amount observed during the tests. It is observed in the test results that, in all of the 1200°F tests, about 2/3 of the total extension occurs in the first five cycles. The prediction for the first five cycles, then, is about 1.5 times that obtained on test.

Prediction of rupture life usage shown in item 14 of Table 27 is based on average measured properties of the material. No prediction could be made on code-established properties because usable properties are not yet established.

Prediction of allowable fatigue cycles shown in item 15 of Table 27 is based on the curves of Code Case 1331-4. It can be seen that in four of the six tests the total residual strain about equals or exceeds 1/2%, which is the anticipated allowable limit strain for weld material. If the test conditions had simulated biaxial conditions, then the 1/2% in the axial direction of these tests would be expected to result in 1% peak radial strain, which is the anticipated limit strain for parent material.

The relatively small fraction of rupture life usage and fatigue life usage in comparison to the deformation indicates that creep ratchetting deformation may be the most severe restriction on high-temperature component design.

Items 16 and 17 of Table 27 show the deformation which might have been expected under these test conditions if the material had had a yield strength of 14,100 psi equal to the minimum anticipated to be set by the code for this temperature. Time-independent ratchetting would be expected to occur as shown. The time-dependent deformation was estimated by proportioning that of item 13a on the basis of yield strength. It appears that between 2 and 3 times as much total ratchetting deformation would be anticipated under these conditions. This comparison indicates the effect of actual yield strength on the accuracy of predictions based on code allowable properties.

Items 12 and 18 of Table 27 are presented to indicate the amount of enhancement of conventional monotonic creep predicted from axial stress only, which can be expected under creep ratchetting conditions.

H. CONCLUSIONS

The following conclusions can be drawn as a result of analysis and tests to date:

- 1) Predictions of creep ratchetting deformation can be made with a simple analytical method to yield accuracy greater than might be expected when based on material properties derived from monotonic tests. Bree's method yields more reasonable results than that developed herein for this case.
- 2) The test results and the analytical results indicate that relative to other failure mode restrictions in design, the creep ratchetting phenomenon can be the most restrictive limit on design of high-temperature

components, seemingly much more so than creep-fatigue interaction. As has been pointed out in previous correspondence, the allowable strain of 1% apparently can be reached in as few as 20 cycles at seemingly reasonable stress levels.

- 3) For cases where the thermal stresses result from heat conduction, Bree's method is expected to yield acceptably conservative results. For cases where the thermal stresses result from thermal transients, the method of analysis proposed herein is expected to be particularly worthwhile as a preliminary design tool, which appears to yield conservative results. When design criteria have progressed to where the time-dependent material properties are established, then the computer program could be used to derive sets of design curves for materials as a function of operating temperature, β and γ . Until then, the use of the simple computer program can yield results quite efficiently when facilities for its use are available.
- 4) Control systems and plant operational procedures can be used to decrease the amount of ratchetting expected in service by proper variation of pressure stresses.
- 5) Use of this program can be especially beneficial to persons involved in materials testing to help guide the selection of test parameters and conditions.
- 6) The amount of creep ratchet strain per cycle decreases significantly as the test progresses. The axial extension-time relations indicate that in the 1200°F tests 2/3 of the total strain occurred in the first five cycles. Tests of isolated specimens, instrumented to observe stress-strain-time relations, and subjected to a controlled load and strain history typical of one of the layers of a wall under creep ratchetting condition, could yield valuable information on the relative contribution of creep rate changes and change in time-dependent stress-strain relations (work hardening) to this change in ratchetting behavior.
- 7) It appears that the ratchetting phenomenon can be reasonably well modeled by substituting temperature for time as predicted. This will allow more confidence in the results of accelerated component testing.

I. FUTURE WORK

Continued testing during the next fiscal year will emphasize the following studies:

- 1) Tests at lower temperatures will be performed in attempting to verify the method of selecting the lower cutoff temperature for significant creep ratchetting.
- 2) Tests at lower temperatures and short hold times will be performed to demonstrate the cutoff range of parameters below which short-time plastic ratchetting is not significant.
- 3) Tests with variation in the axial stress with time will be performed. The axial stress is related to the pressure stress in components and the results of these tests will be used to develop and verify predictions as to the ways in which system control and plant operation can ameliorate the creep ratchetting problem.
- 4) Tests will be made on specimens from material at the lower extreme of tensile and creep properties. This is to attempt evaluation of the effect of the range of materials properties on ratchetting deformation.

Analyses will be performed of results to date using more sophisticated computer programs with stress conditions and results more directly related to the test conditions than the approximate methods used to date.

J. DECISIONS

The most important decision for which these tests have provided background is that of making consideration of creep ratchetting a mandatory requirement in the new Code Case 1331-5 for high-temperature design. The results have also helped to verify the simplified code rules for the lower limit of ratchetting behavior.

It has also been decided on the basis of the results of this effort that the first piping test in the SCTL should emphasize study of the effects of creep ratchetting under thermal transients.

K. EVALUATION

The results of this modest effort have indicated that creep ratchetting invokes probably the most severe limitation on the structural capability of LMFBR power plants and also that it can be evaluated sufficiently well for design purposes with material properties and design analysis methods readily made available.

The results do not contribute to a basic understanding of the behavior of the material under these conditions, but contribute to understanding of structural behavior.

L. PROBLEMS

The major problem encountered to date in this program is the inefficiency and inaccuracy of the strain measurement method using a telescopic microscope. This problem is under study, as discussed in Appendix B.

The long time duration of the tests is a problem which may have to be accepted, unless more machines are built.

M. SUGGESTIONS

The following suggestions are offered to improve the performance of this program and to improve the ability to predict creep ratchetting deformation:

- 1) Effort should be spent on development of high-temperature strain measuring devices emphasizing large strains ($\sim 1\%$) and long time stability. Devices other than electric resistance strain gages should also be considered.
- 2) Creep data should be developed for materials in the lower temperature ranges of creep, 800 to 1000°F. Measurements of primary creep are required for times on the order of 1000 hr and at stresses in the range of 50 to 125% of the yield stress. Far too many tests are performed at high temperatures and at stresses far above the yield stress to acquire the maximum amount of data (useful or not) in the minimum time.
- 3) Other, more fundamental, programs should be undertaken which allow measurement of the changes in stress-strain-time-temperature relations under and during creep ratchetting conditions.

- 4) Analytical methods should be checked against results to date. Variation in the parameters should be made to determine those which exhibit the greatest effect and thus help guide the expenditure of funds for further research on this subject.

N. REFERENCES

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APPENDIX A

CREEP RATCHET DEFORMATION CALCULATION PROGRAM

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OLD:CRATD3
5$NDM
10C*** CREEP RATCHET DEFORMATION CALCULATION PROGRAM
20 PRINT 10
30 10 FORMAT(////10X,"CREEP RATCHET DEFORMATION CALCULATION
31& PROGRAM",
35& // "FIRST CASE USING EDOT = (SIG/SEO)**XE AND TR=(SRO/SIG)**XR")
36 5 PRINT,"TYPE IN E,SEO,XE,SRO,AND XR",1*
37 READ,E,SEO,XE,XK,XN
40 20 PRINT,"TYPE IN MEAN & YIELD STRESS,PHI & PSI",1*
50 READ,S0,SY,FI,PSI
51 IF((S0.LE. 0)) GO TO 60
54 URATE = (S0/SEO)**XE
55 SIG0 = S0 + PSI * SY
56 SIGFF = 1.1*S0
105 RT = (1. + FI ) / FI
110 PRINT, "TYPE IN NO. LINES OUTPUT",1*
120 READ, IPC
170 IF(IPC.LE. 2) IPC=2
180 ISIG=SIG0
190 T=0
200 IPC=(SIG0-SIGFF)/(IPC-1)
210 SIG=SIG0
220 DAM=0
230 EPS=0
235 EPRCH = 0
240 I=0
250 EPS2=0
260 30 CONTINUE
265 SE = SIG/SEO
270 SIGPR=SIG
280 SIGF = (RT*S0 - (RT - 1.)* SIG)
290 SIGF2=ABS(SIGF)
291 SE2 = SIGF2/SEO
292 SIGN = SIGF/SIGF2
295 RATE = (SE**XE - SIGN*SE2**XE)/RT
296 DELT = .02 * SIG /( E * RATE)
300 DELSIG = -E * RATE * DELT
305 DELRCH = SE**XE * DELT
310 DELEPS = DELRCH + DELSIG/E
320 SIG=SIG+DELSIG
330 T=T+DELT
335 USTRN = T * URATE
340 EPS=EPS+DELEPS
345 EPRCH = EPRCH + DELRCH
350 SIGAVG=(SIG+SIGPR)/2.0
370 TR=(XK/SIGAVG)**XN
380 DAM=DAM+(DELT/TR)
400 EPS4=DELEPS/DELT
410 PRNT=ISIG-I*IPC
420 IF(SIG.LE.PRNT) GO TO 40
430 GO TO 30
440 40 PRINT 45,T,DELT,USTRN
450 45 FORMAT(// "T="F11.3,5X,"DELTA T="F11.3,5X,
451& "CREEP="IPIE10.4)
455 I=I+1
460 PRINT 50, SIG, EPS,DELSIG, DELEPS, SIGAVG
465 50 FORMAT(6X,"STRESS",6X,"STRAIN",6X,"DEL STRESS",2X,
467& "DEL STRAIN",2X,"AVG STRESS"/1X,1P5E13.5)
470 PRINT 55,EPS4,TR,DAM,DELRCH,EPRCH
475 55 FORMAT(3X,"STRAIN RATE",1X,"TIME TO RUPT",3X,"DAMAGE",
477& 4X,"DEL RATCHET",5X,"RATCHET",3X/1X,1P5E13.5)
480 IF(SIG.GT.SIGFF) GO TO 30
485 PRINT 57
500 57 FORMAT(//// "NEW CASE - TYPE IN 0. TO TERMINATE")
510 GO TO 20
520 60 STOP
530
1000 END

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READY

APPENDIX B

EVALUATION OF A SPECIALIZED EXTENSOMETER

In an effort to supplement or replace the present optical measuring system on the creep-ratchet experiments, an elevated-temperature extensometer was obtained on a one-year loan basis from Edwards Air Force Base. This extensometer was developed for NASA by the Boeing Company. All particulars on this extensometer are contained in NASA publication N69-77051. The unit was developed originally to operate in an atmosphere of 2000°F by using water cooling.

Briefly, the extensometer sensing system consists of two opposed buckled beams acting, through a cross arm, about a knife-edge pivot. Two back-to-back strain gages are bonded to each buckled beam and are connected in a full bridge arrangement. Strain is transmitted to the sensing system through two posts with points of sapphire pins (one post is fixed, the other movable) that are made to bear in the test-specimen target holes. The nominal gage length of this extensometer is 0.5 in.

A sample specimen (316 SS bar, 1/8-in. thick) was prepared with a series of target holes at 0.5-in. intervals to check out the repeatability of the extensometer. The target holes were punched with a 120-deg-angle Rockwell-C hardness indenter at a depth between 0.01 and 0.015 in. Numerous measurements were made, and about the best repeatability achieved between any one set of targets was about 0.002 in. It was decided that the 120-deg indenter point (measured at 0.01-in. diameter) and the extensometer sapphire points were too dull, and were not bottoming out in the same position each time.

A different type of indenter and measuring points were then tried. The indenter used was a 90-deg diamond, and two 60-deg phonograph needle diamonds were mounted on the extensometer. In the preliminary tests with this setup, various extensometer contact pressure and insert angles were tried. In these the repeatability did not improve significantly from the original setup, but inspection of the points and target holes showed that the tips of the extensometer points were broken off again and the target holes were badly cut up. The tips of the extensometer points were broken off due to angling the extensometer in the holes in an attempt to bottom out. However, it does appear that with proper handling and measuring techniques, this system will work with an acceptable degree of accuracy.

Another preliminary test specimen was made by indenting four measurement holes at 0.495, 0.5, 0.505, and 0.5 in. Ten measurements were made at each set of targets. The specimen was then heated at 1200°F for 50 hr, was cooled, and ten more measurements were made at each set of targets. This sequence will approximate actual conditions in the tests to be conducted. The results of this test indicate a root mean square value of the ten measurements of roughly 150 microinches when the large errors are thrown out. The difference between means of measurement between any two points was of this same magnitude.

A calibration device to determine the accuracy of the extensometer will have to be built. This calibrator will be used as a reference frequently during test specimen measurements, and will be traceable for accuracy to the National Bureau of Standards.

Because the present diamond points on the extensometer pins are chipped, it was decided to use 90-deg diamond points in lieu of the 60-deg points, and to use a 120-deg diamond indenter lapped to a 0.001-in.-diameter tip to punch the target holes. The 90-deg diamond tip measures 0.0008 in. in diameter. This switch is expected to give more durability of the extensometer points, and will cause less wear and tear on the target holes. Of special concern is the calibrator, which will be constantly used. An order has been placed to change these points, and preliminary tests will be conducted when the changeover is made.

One major benefit expected from this development is improved measurement efficiency. At present the readings may be printed out directly in microinches. Facilities may be available for having the data also punched out directly on paper tape for direct data processing by a computer.