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Contract AT(10-1)-880

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ARMY GAS-COOLED REACTOR SYSTEMS PROGRAM

Quarterly Progress Report
1 October through 31 December 1963

15 February 1964

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AEROJET-GENERAL NUCLEONICS
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ARMY GAS-COOLED REACTOR SYSTEMS PROGRAM

QUARTERLY PROGRESS REPORT

1 OCTOBER THROUGH 31 DECEMBER 1963

Published

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Report No. IDO-28621

ARMY GAS-COOLED REACTOR SYSTEMS PROGRAM

QUARTERLY PROGRESS REPORT*

1 October through 31 December 1963

ABSTRACT

This document summarizes the technical progress of the Army Gas-Cooled Reactor Systems Program under Contract AT(10-1)-880 between the USAEC and Aerojet-General Corporation. The purpose of the Program is to develop a mobile, low-power, nuclear power plant capable of extended operation under military field conditions.

The ML-1 power plant did not operate during the report period; low power reactor physics and shielding experiments were conducted with the ML-1 reactor.

Evaluation of moderate corrosion observed on aluminum parts exposed to the ML-1 shield solution indicated no loss of performance capability. Preliminary tests showed that the corrosion probably was caused by heavy metal ions or chlorides in the solution. Massive corrosion observed on the ML-1 fuel element lower spiders was attributed to sub-standard material; failure of some spiders was attributed to a combination of corrosion and sub-standard fabrication. Evaluation indicated that the upper spiders will perform satisfactorily for the design lifetime.

Modification, repair and reassembly of the CSN-1A t-c set was completed. Operation demonstrated bearing stability, but showed that the turbine effective flow area was too large. A bypass flow path in the turbine was being corrected at the end of December. The TCS-670 t-c set will be stored indefinitely.

Since a commercial alternator will be used for the ML-1A, further development of the brushless alternator was postponed indefinitely. Evaluation revealed that the ML-1 improved precooler design was not compatible with ML-1A requirements; proposal requests for an ML-1A precooler were issued late in December.

Operation of the IB-17R-2 and -3 test elements in the GETR continued without incident.

Preliminary design of the ML-1A power plant was initiated; completion is scheduled for May 1964. Design of modifications to the GCRE facility to adapt it to testing the ML-1 reactor skid was initiated; completion is scheduled for 31 January 1964.

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ARMY GAS-COOLED REACTOR SYSTEMS PROGRAM

QUARTERLY PROGRESS REPORT*

1 October through 31 December 1963

SUMMARY

This document summarizes the technical progress of the Army Gas-Cooled Reactor Systems Program (AGCRSP) under Contract AT(10-1)-880 between the USAEC and Aerojet-General Corporation. The purpose of the Program is to develop a mobile, low-power, nuclear power plant capable of extended operation under military field conditions.

The ML-1 power plant did not operate during the report period, but the reactor was operated at low power levels to conduct reactor physics and shielding experiments.

Evaluation of the moderately severe corrosion observed on aluminum parts exposed to the borated shield solution in the ML-1 reactor indicated that the performance of the parts would not be affected provided the corrosion can be arrested. A laboratory test program was initiated to establish the cause of the corrosion; preliminary results indicate that the presence of heavy metal ions or chlorides in the solution may be responsible for the corrosion.

Massive corrosion was observed on the lower spiders of the ML-1 fuel elements and some of the spiders had failed. Metallurgical evaluation attributed the corrosion to the below-specification chromium content of the spiders, and the failures to weakening of the spiders by a combination of corrosion and substandard fabrication. The development of replacement spiders, including design and testing, was completed and parts were being fabricated at the end of December for installation early in January. An evaluation of the condition of the upper spiders indicated that these components will perform satisfactorily for the 10,000 hr design lifetime.

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Modification, repair and reassembly of the CSN-1A turbine compressor set was completed. The stability of the bearing system was demonstrated during 18 hr of operation and post-test inspection revealed no significant bearing wear. Additional tests to evaluate the effect of variations in lubricating oil temperature and to accumulate thermodynamic data confirmed the suitability of the bearings, and indicated that the turbine effective flow area was too large. Post-test inspection revealed a bypass flow path in the turbine which was being corrected by a minor modification at the end of December.

An evaluation of the Stratos proposal for modification and rebuild of the TCS-670A turbine-compressor set and of the possible use of the set in the ML-1 program resulted in the decision to store the unit indefinitely.

Tests of the completed alternator in the structural plastic case resulted in bearing failures which were subsequently attributed to unbalanced forces created by the laminations in the driven end housing. In consideration of the decision to use a commercial alternator for the ML-1A, further development of the ML-1 brushless alternator was postponed indefinitely.

Evaluation of the ML-1 improved precooler design revealed that the unit is not compatible with ML-1A requirements. The direction of this program was altered to permit the procurement and testing of a precooler which is acceptable for ML-1A. The specifications for such a unit were transmitted to prospective vendors at the end of December with requests for proposals and quotations.

The IB-17R-2 and -3 test elements continued to operate in the GETR without incident.

The preliminary design of the ML-1A power plant was initiated during the report period. Design work proceeded throughout the period; the estimated completion date for the preliminary design is May 1964.

The design of modifications to the GCRE facility to permit testing of the ML-1 reactor skid in that installation was initiated on 1 November 1963. Title II Design is scheduled for completion on 31 January 1964.

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ARMY GAS-COOLED REACTOR SYSTEMS PROGRAM

QUARTERLY PROGRESS REPORT

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ARMY GAS-COOLED REACTOR SYSTEMS PROGRAM

QUARTERLY PROGRESS REPORT*

1 OCTOBER THROUGH 31 DECEMBER 1963

I. PROGRESS TO 1 OCTOBER 1963 - SUMMARY

The Army Gas-Cooled Reactor Systems Program evolved from studies conducted at ORSORT in 1954 and by Sanderson-Porter Co. in 1955 to evaluate the feasibility of the development of a mobile nuclear power plant for military use. These studies indicated the feasibility of such a concept and established the basic objective of the Program. This objective was the development of specifications for a mobile, low-power nuclear power plant capable of extended operation under military field conditions. The development programs for the reactor and power conversion equipment started in late 1956 and Aerojet was selected as the systems contractor to integrate all Program activity in 1959. The following major projects have been undertaken:

1) The design and construction of a reactor test facility (GCRE) at NRTS, Idaho: This work began in early 1959 and was completed late in 1959.

2) The design and construction of a turbine-compressor test facility (GTTF) at Fort Belvoir, Virginia: This work was completed in 1959.

3) The design, fabrication and test operation of a test gas-cooled reactor (GCRE-I): This test reactor was provided in the program to investigate the operational and control characteristics of the reactor concept chosen for the power plant, to provide information on system transients for use in designing the plant, and to provide for developmental and life testing of fuel elements. The reactor was heterogeneous, water-moderated and nitrogen-cooled, and operated at a nominal thermal power of 2.2 Mw. The reactor was first taken to criticality in February 1960 with plate-type fuel elements and operation continued with these and with a replacement core of pin-type elements (prototype for the power plant) until April 1961 when the reactor was shut down after a

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failure in the reactor calandria. Investigation of the failure continued for the balance of 1961. The decision was made early in 1962 to deactivate the GCRE-I and the GCRE was placed in stand-by condition. Late in 1963, a program to develop the design of modifications to the GCRE to permit testing of the ML-1 reactor skid without power conversion equipment was initiated (see Section 5.0).

4) The design, fabrication and test operation of a developmental t-c set (TCS-560): This unit was delivered late in 1959 and tests and modifications of the set continued until May 1963. At that time, Aerojet was relieved of cognizance for the developmental program.

5) The design, development, and fabrication of two core loadings for the reactor of the demonstration power plant (ML-1): The design, development and fabrication of the first core loading (ML-1-I) was completed early in 1961 and this core has been in operation in the ML-1 since that time (see Section 2.4). The design and development of the second core loading (ML-1-II) is in progress (see Section 3.4).

6) The design and fabrication of two turbine-compressor sets for the demonstration power plant:

a) <u>TCS-670 (Stratos)</u> — This set was delivered early in 1961. Preliminary testing revealed that the unit did not satisfy design specifications and modification was undertaken in 1963. In open cycle tests of the modified unit, the unit again failed because of insufficient internal clearances for thermal expansion. After additional modifications the unit was successfully open-cycle tested but failed (seized) during subsequent closed cycle tests. Activity associated with this machine in the current quarter is discussed in Section 3.2B.

b) <u>CSN-1 (Clark)</u> — This set, delivered in March 1961, was installed on the power-conversion skid after preliminary testing and delivered to NRTS for testing with the demonstration power plant in June 1962. The unit performed acceptably during ML-1 testing in September 1962 and February 1963 although the power output was less than the design value. Inspection following the February run revealed abnormal bearing wear and some cracking in the turbine blades. The program of modification and repair is discussed in Section 3.2a.

7) The design and construction of a test facility for the demonstration power plant at NRTS-Idaho was completed late in 1960. Modifications to the facility were initiated in the current quarter and are discussed in Section 1.0.

8) The design, fabrication and test operation of a demonstration power plant (ML-1): The ML-1 control cab and reactor skid were delivered to NRTS in February 1961 and the reactor achieved initial criticality on 30 March of that year. Operational tests to verify predictions of control rod worth, reactivity temperature coefficients and shielding effectiveness, and to develop general core physics data were conducted from

April 1961 to June 1962. With the arrival of the power-conversion skid (see 6-b above), the final checkouts were completed and initial operation of the power plant was achieved in September 1962. Following this operation, the plant was shut down for modification and maintenance. Test operations were resumed in January 1963. During these tests, the ability of the reactor to operate at 3.3 Mw(t) — full design power — was demonstrated and a shaft output power of 247 kw(e) was measured. Following this testing, evidence of a leak in the reactor pressure vessel was observed and, after confirmation of the leak, the reactor skid was partially disassembled, the leak was repaired and the skid was reassembled. Additional activities associated with the repair and modification of the plant are presented in Sections 1.0, 2.0, and 3.0 of this report. The ML-1 plant characteristics are presented in Appendix A.

9) The development of performance specifications for a field-operable, gas-cooled nuclear power plant based on the ML-1 design (ML-1A): This work was completed on 30 June 1963.

10) The completion of a design study for a "second generation" gas-cooled mobile nuclear power plant (ML-2): Preliminary feasibility studies of advanced concepts were begun at a modest level of effort in 1961. The first phase of the work was completed in March 1962, at which time a more detailed evaluation was initiated. The goal of this evaluation was to define a 500 kw(e) power plant with minimum weight, maximum reliability and maintainability, minimum logistic support requirements, and minimum startup and relocation times. The report of the preliminary studies was published in September 1962 and the final report of the detailed study was published in October 1962. A limited evaluation of a reactor concept not fully considered in the earlier work was performed in May and June 1963, at the direction of the USAEC.

11) The preparation of a preliminary design for a field-operable, gas-cooled nuclear power plant (ML-1A) based on the ML-1A performance specification (item 9 above): This work was initiated in mid-1963. Progress in this activity is discussed in Section 4.0.

The work summarized above was conducted under several contracts. The principal active contract at this time is Contract AT(10-1)-880 between the USAEC-ID and AGC. The repair of the CSN-1 t-c set is being performed under Contract DA-44-192-ENG-8 between USA-ERDL and AGC and is the only remaining activity under that contract.

This report is organized under four primary headings: Summary of Progress to 1 October 1963, ML-1 Project, ML-1A Project and GCRE Modification. Significant areas of activity are identified by numbers 1.0 through 5.0 (second order identification) and details are reported as decimals of the second order identification. The figures and tables are identified with the appropriate second order heading and are included in the text close to the point of reference. Two kinds of references are cited: Alphabetical designations refer to reports which received general distribution; numerical designations refer to in-contract reports.

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II. ML-1 PROJECT

1.0 ML-1 TEST OPERATIONS

The ML-1 power plant was not operated during the report period. At the end of September, the repair and reassembly of the reactor had been completed, the skid had been moved to the ML-1 test facility at NRTS, the instrumented fuel elements and new reactivity shims had been installed and the reactor components and instrumentation were being checked out prior to refueling of the reactor (Ref. a)*.

An inspection of the fuel elements was completed early in October prior to reactor refueling. This inspection revealed that the lower spider of five of the elements was deformed or broken. This observation resulted in a significant program of redesign and modification of the fuel elements (see Section 2.4).

Spare fuel elements were substituted for those with damaged lower spiders and the reactor was refueled to permit the conduct of several low-power experiments. In general, the goals of this experimental program were to establish the operability of the nuclear instrumentation, to develop data concerning the nuclear characteristics of the repaired pressure vessel, and to provide shielding performance data for use in the ML-1A preliminary design. The experiments performed to determine the reactivity of the core by measurements of critical control rod position indicated that a decrease of approximately 0.5% Δ K/K had occurred between March and November 1963. Subsequent analysis revealed that this decrease was associated primarily with the increased silver content of the new reactivity shims. The reactivity shim pattern was revised by replacing six silver-plated shims with six stainless steel shims. Calculations indicate that the excess reactivity of the core following this change should be about 1.65% Δ K/K (see Section 2.4).

All 61 fuel elements were removed from the reactor and transferred to the GCRE pool in mid-December in preparation for replacement of the lower spiders. The purity and pH of the GCRE pool water was controlled throughout the report period to minimize the corrosion of the components stored in the pool; the resistivity of the pool water was maintained above 1.0 megohms and the pH was controlled between 5.8 and 7.0.

*References are listed after the main body of the text. Alphabetical designations refer to reports which received general distribution and numerical designations refer to in-contract documents.

A test to evaluate the capability of the cyclone separator, desiccant bed and refrigeration system on the power conversion skid to remove oil vapor from the gas being returned to the main coolant loop was completed during the report period. Evaluation of the data obtained during the test indicates that the oil removal system is capable of adequately controlling oil concentrations (i.e., maintaining less than 1 ppm in 170°F gas).

Several tests were performed in support of the ML-1A design effort to determine the volume and distribution of the cooling air flow through the precooler. An endurance test of the ML-1 non-prototype start motor was completed during the period. The motor operated without any problems for more than 97 hours; vibration readings throughout the test were similar to data taken during the original acceptance tests.

Work continued on the modification of the ML-1 test building to improve the efficiency and testing capability of this facility. Construction of an auxiliary building to house the equipment for mixing shield solution was begun.

2.0 ML-1 OPERATIONS ENGINEERING SUPPORT

2.1 Reactor and Auxiliaries

a. Corrosion of Aluminum: It was noted during repair of the pressure vessel (Refs. a and b) that aluminum parts were unusually difficult to weld after exposure to the borated shield water solution. Examination of samples taken from the repaired parts revealed pitting and intergranular corrosion to an average depth of 0.010 to 0.015 in. A four-part program was initiated to establish the cause and operational importance of this corrosion, and to define any required corrective action. This program included:

- Examination of additional samples of aluminum from various parts of the shield water system
- A literature search and a review of earlier ML-1 laboratory corrosion tests
- A laboratory testing program to establish the cause of the observed intergranular corrosion
- An evaluation of the materials used and the configuration of all components of the ML-1 reactor skid which are exposed to the shielding solution

1) Examination of Additional Samples: Additional samples of aluminum obtained from various parts of the shield water system (Fig 2-1) were metallurgically examined to assess the extent of corrosion in the system. The examination (summarized in Table 2-1) indicated that corrosion had occurred on all aluminum parts exposed to the borated shield water. The depth of corrosion ranged from 0.010 to 0.015 in. in the 6061 and 6063 series aluminum except for samples from the removable upper surge tank which had less exposure to the borated water than other parts of the system. It was noted that purer aluminum alloys (i.e., 1100 and 3003 series) were less severely corroded than 6061 aluminum. No signiciant corrosion was found in aluminum after contact with the moderator water nor were stainless steel parts corroded. Representative photomicrographs of intergranular corrosion on aluminum parts in the shield water system are shown in Figs 2-2 and 2-3.

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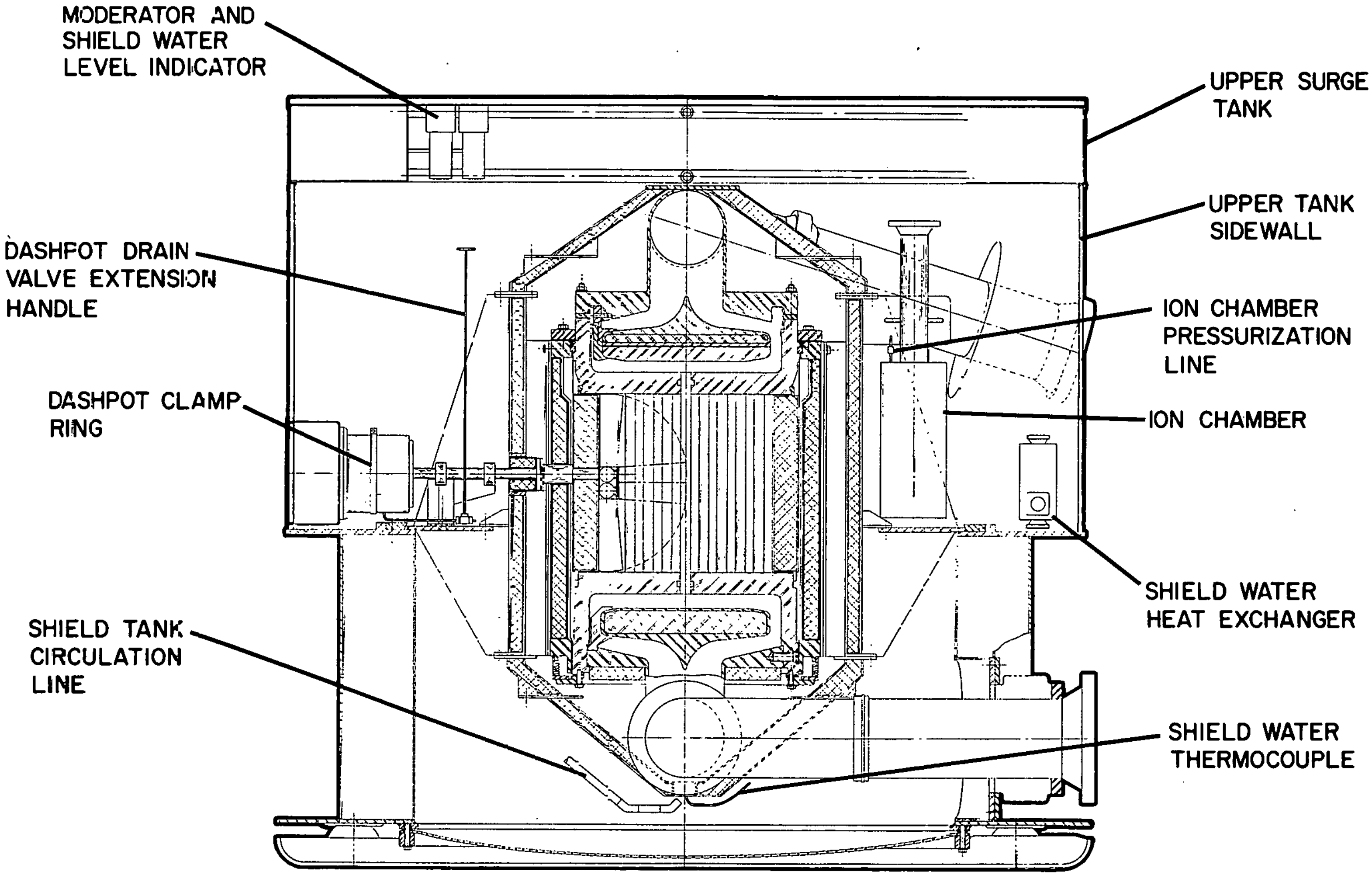


FIGURE 2-1. THE ML-1 REACTOR PACKAGE (SHOWING SITES FROM WHICH CORROSION SAMPLES WERE REMOVED)

TABLE 2-1 - METALLOGRAPHIC SUMMARY OF SHIELD WATER/ALUMINUM CORROSION

Sample Location	Material	Service	Condition of Sample
Ion Chamber Pressurization Line	6061-T6 Aluminum	Shield water outside; nitrogen inside	Intergranular corrosion; 0.010 to 0.015 in. average penetration on outside only
Shield Tank Circulation Line	6063-T6 Aluminum	Shield water inside and outside	Intergranular corrosion; 0.010 to 0.015 in. average penetration on both inside and outside surfaces
Shield Water Heat Exchanger Outer Fin	3003 Aluminum	Shield water (static)	Intergranular corrosion up to 0.005 in. deep
Shield Water Heat Exchanger Inner Fin	3003 Aluminum	Shield water (moving)	Minor corrosion
Shield Tank Circulation Line Weld Deposits & Base Metal Interfaces	6063-T6 Aluminum	Shield water inside and outside	Extensive intergranular corrosion
Grayloc Seal Rings (4 in.)	6061-T6 Aluminum	Shield water outside; moderator water inside	Intergranular corrosion on outside; some evidence of stress
Grayloc Seal Rings (2 in.)	6061-T6 Aluminum	Shield water inside and outside	Minor corrosion inside and outside; stressing evident
Moderator Water Level Indicator Conduit	6061-T6 Aluminum	Shield water outside; nitrogen inside	Intergranular corrosion 0.002 to 0.003 in. deep on outside only
Shield Water Level Indicator Conduit	6061-T6 Aluminum	Shield water outside; nitrogen inside	Intergranular corrosion 0.002 to 0.003 in. deep on outside only
Dashpot Clamp Ring	6061-T6 Aluminum	Shield water on all surfaces	Intergranular corrosion 0.010 to 0.015 in. deep
Shield Water Thermocouple Sheath	1100 Aluminum	Shield water on outside	Extensive pitting; corrosion 0.003 in. deep; no intergranular corrosion

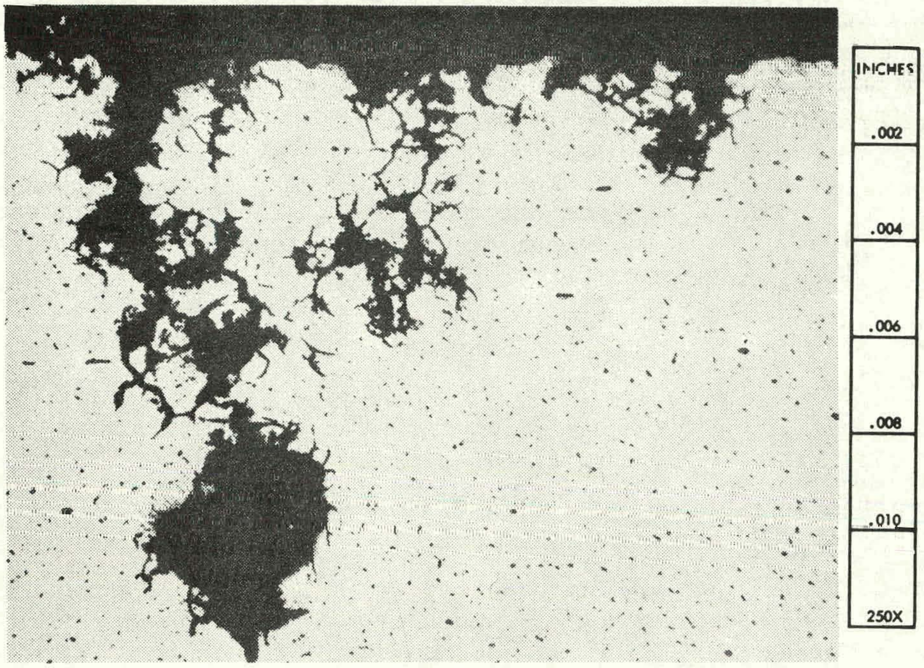


FIGURE 2-2. REPRESENTATIVE ALUMINUM CORROSION

6061-T6 Aluminum from the Ion Chamber Pressurization Line

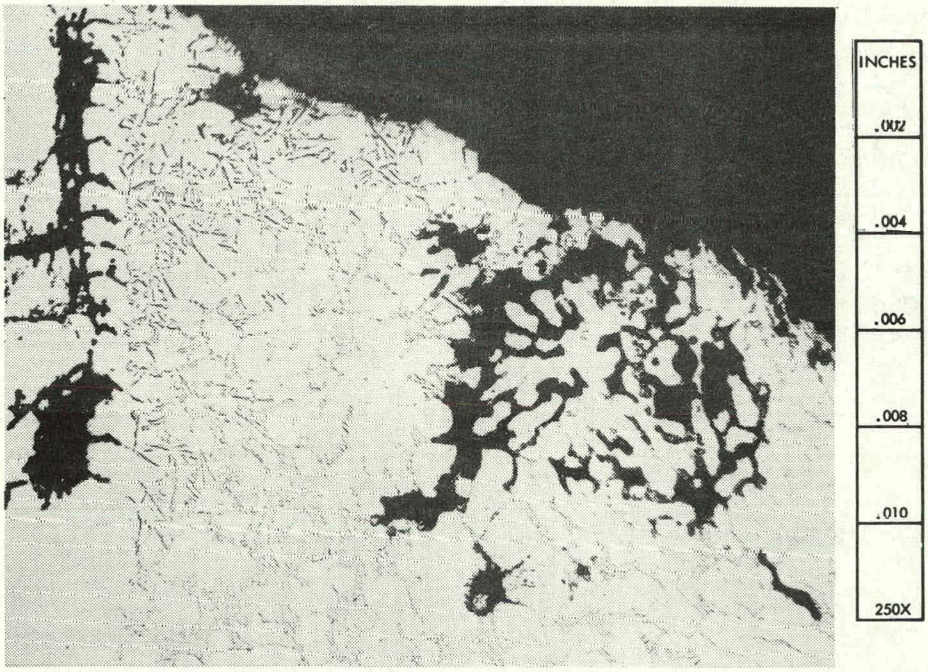


FIGURE 2-3. REPRESENTATIVE WELD DEPOSIT CORROSION

4043 Weld Deposit on 6063-T6 Aluminum from the Shield Water Tank Circulation Line

2) Literature Survey and Test Review: A literature survey and a review of earlier corrosion tests conducted under the AGCRSP were completed to develop basic information on the corrosion of aluminum in boric acid solutions. Information obtained from aluminum manufacturers and from the literature (Refs. c and d) indicated that aluminum is not appreciably affected by boric acid solutions, but the presence of heavy metallic ions (from such impurities as copper and lead) will accelerate the corrosion. The 500-hr laboratory corrosion test of 6061 series aluminum in hot solutions of reagent grade boric acid, completed earlier in the Program, revealed uniform corrosion at a rate of less than 0.001 in./yr.

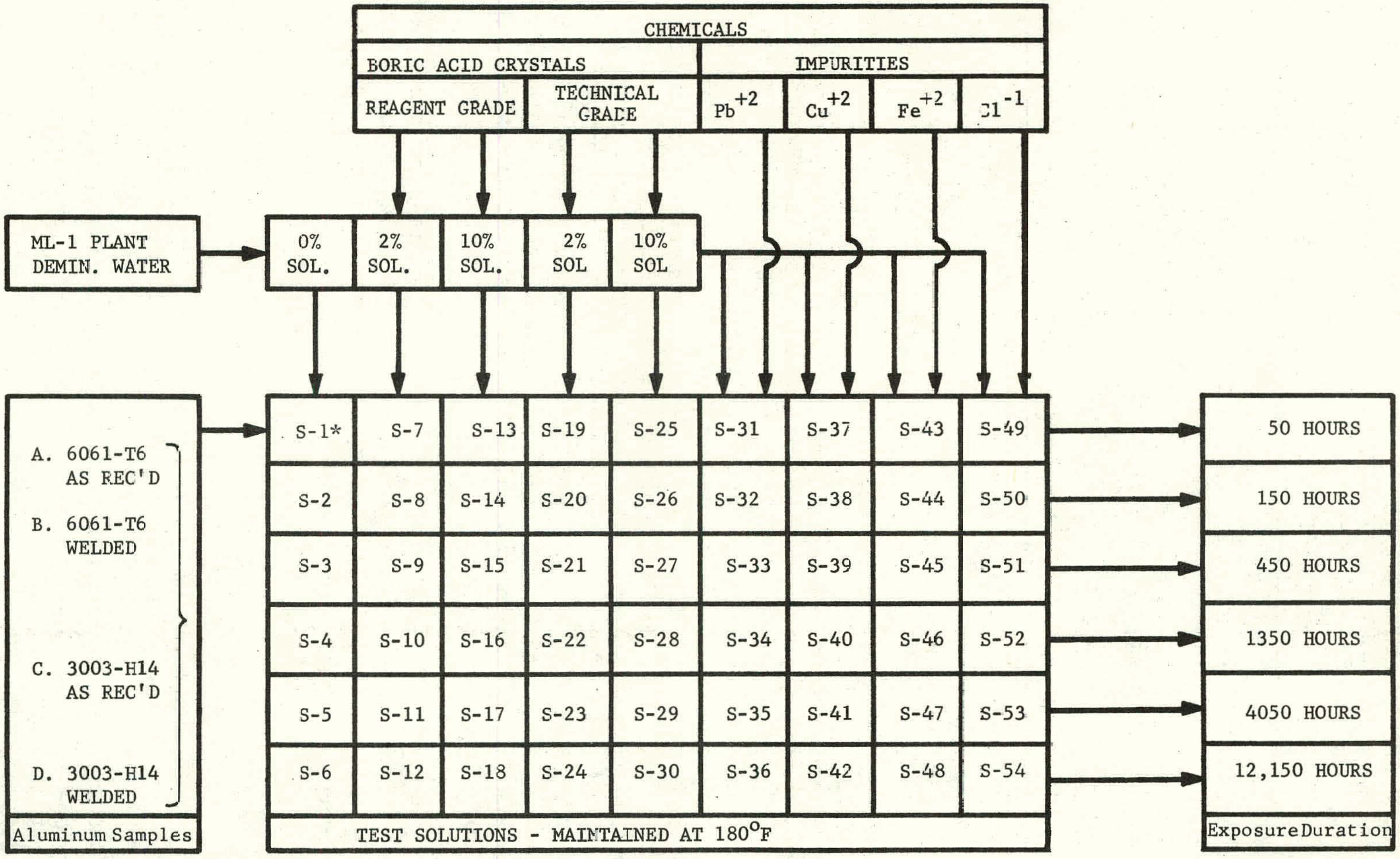
3) Laboratory Testing Program: A test program to establish the cause of the intergranular corrosion observed in the ML-1 reactor was initiated. This program will expose samples of 6061-T6 and 3003-H14 aluminum in the as-received and as-welded conditions to nine different solutions, for different periods of time (Fig 2-4) at 180°F. The solutions include ML-1 plant demineralizer water without additives, 2% and 10% solutions made from plant demineralizer water and reagent grade boric acid, 2% and 10% technical solutions made from plant demineralizer water and technical grade boric acid, and all four types of boric acid solutions with added impurity ions (Pb^{+2}, Cu^{+2}, Fe^{+2} and Cl^{-1}) at concentrations of about 5 ppm.

The 50-, 150- and 450-hr tests had been completed by the end of December. These tests revealed that copper and chloride impurities cause intergranular corrosion of 6061 series aluminum (Fig 2-5 and 2-6). The effect of lead impurities has not been conclusively demonstrated thus far, but the tests show that the presence of iron ions does not significantly effect the corrosion rate of aluminum. These tests also show that reagent grade boric acid solutions are less corrosive than technical grade solutions, and that the 3003 series aluminum is considerably more corrosion-resistant than 6061 series aluminum.

Tests are being run with solutions of various concentrations of impurities to determine the levels of copper, chloride and lead impurities that can be tolerated in the ML-1 shield water system without excessive corrosion. These tests will run for 450 hr.

4) Component Evaluation: The materials of construction and the configuration of all components of the ML-1 reactor skid, which are exposed to the shielding solution were studied. Based on the extent of the corrosion observed to date, it was concluded that all components would perform satisfactory for the design lifetime, provided techniques could be developed to significantly reduce the rate of corrosion.

b. Seven-Tube Mock-up Test: Lifetime testing of the seven-tube mock-up of the ML-1 calandria in oxygenated water was resumed early in October after the main blower bearings had been replaced following the failure in September (Ref. a). The purpose of this test is to evaluate the resistance of the ML-1 calandria to corrosion from oxygenated water at normal operating conditions and, thus, to establish if a requirement exists for deoxygenation of the ML-1 moderator water. The test was interrupted in November when a belt failed, damaging the blower impeller shaft. A new shaft was fabricated and installed. By the end of December, 3932 hr of testing had been accumulated. About 2180 hr of testing



*One Sample of Each Material (A, B, C & D) was Exposed in Each Solution (S-1, S-7, etc).

FIGURE 2-4. LABORATORY TESTING PROGRAM: CORROSION OF ALUMINUM

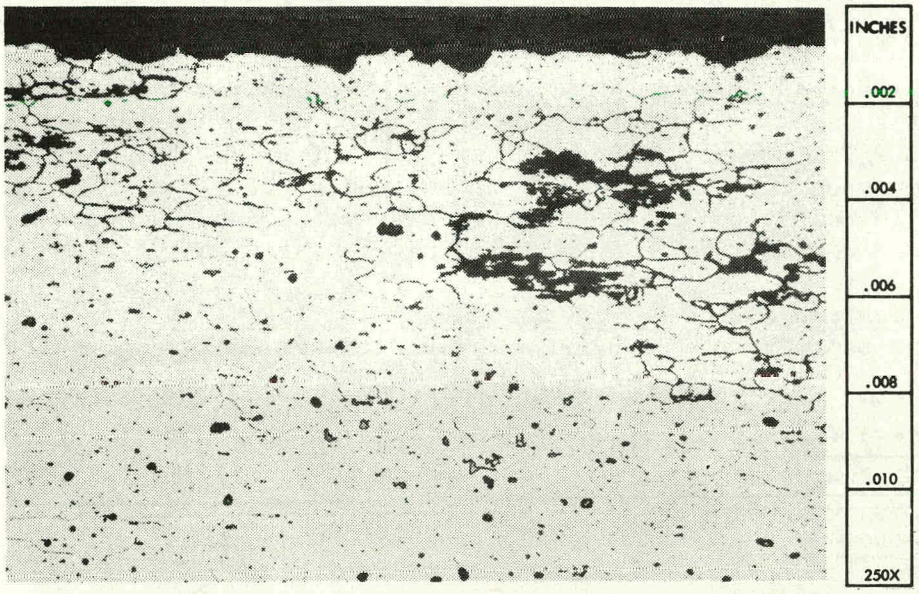


FIGURE 2-5. INTERGRANULAR CORROSION IN TYPE 6061-T6 ALUMINUM EXPOSED 150 HOURS IN CHLORIDE-BEARING TEST SOLUTION. ETCHED: LIGHT, 1% HF

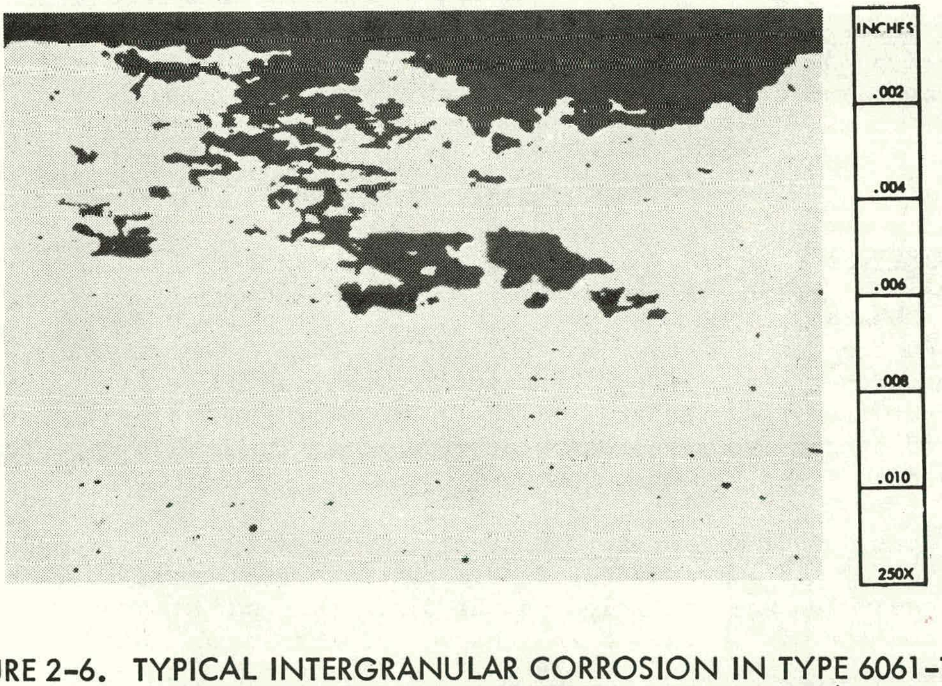


FIGURE 2-6. TYPICAL INTERGRANULAR CORROSION IN TYPE 6061-T6 ALUMINUM EXPOSED 50 HOURS IN COPPER-BEARING TEST SOLUTION. UNETCHED

have been accumulated since the mock-up was last inspected; the next inspection (after 2500 hr) is scheduled for 15 January 1964.

c. Aqueous Shield System: Procurement of the materials and parts for the shield water handling system was completed and the equipment was shipped to the ML-1, NRTS. The purpose of the system is to minimize contamination of the ML-1 test building during mixing and to improve heating, mixing and transfer procedures. Installation and checkout of the system is dependent on the completion of the addition to the ML-1 Test Building (see Section 1.0).

d. Deoxygenation System Heat Exchanger Modification: Modification to improve the cooling capacity of the heat exchanger in the deoxygenation system was completed.

e. Moderator Temperature Control: Fabrication of aerodynamically balanced louvers for the moderator cooler was completed during the period. These louvers are designed to close completely regardless of air loading and thus permit better control of the moderator temperature. Evaluation of the performance of the louvers at the Power Conversion Test Facility (PCTF), San Ramon, is scheduled for early January.

f. Gas Flow Measurement: Fabrication of a Venturi meter to provide direct measurement of the flow of process gas in the ML-1 main loop was completed. The design of the turbine inlet ducting which contains and supports the meter was completed, and assembly was initiated. It is anticipated that assembly, proof testing, and calibration will be completed by the end of January 1964.

g. ML-1 Lifting Sling: Several welds on the ML-1 lifting sling failed and the modifications were completed in the previous report period (Ref. a). The modified sling was not load tested during this report period.

2.2 Power Conversion

a. Refrigeration System Temperature Control: Testing of temperature control modifications to improve the performance of the refrigeration system (in the seal gas return system) were completed. The temperature sensing element installed in the gas line between the seal gas heat exchanger and the cyclone separator properly activated the solenoid valve which permits direct bypass of refrigerant from the discharge to the inlet of the refrigeration compressor and eliminated cyclic operation of the compressor. The modified system easily maintained 40^{o}F gas temperature.

b. Lubricating Oil Temperature Control: The modifications required to improve the control of the lubricating oil temperature were completed. The motor-operated bypass valve was installed on the Clark skid and an improved sump temperature controller was installed and tested. The test indicated that the sump oil temperature was maintained within $\pm$ 4^{o}F of the set point by the new controller.

c. Start-Up Compressor: The start-up compressor was operated at three-quarters speed for 100 hours to establish the operability of the compressor and gearbox at the higher speed. If preliminary data, which indicate that the seal gas flow rates on the CSN-1A are significantly lower than those of the CSN-1, are confirmed, the half-speed start-up compressor formerly installed on the skid will be adequate and the modified unit will not be installed.

d. Recuperator Insulation: The thin (0.005 in.) stainless steel thermal shield in the recuperator high pressure inlet line on the Clark skid was replaced with a 0.030-in.-thick Inconel sleeve. A similar modification had previously been made to the Stratos skid (Ref. a).

e. Lubricating Oil Separator Level Control: Operation of the lubricating oil separator level control on the Clark skid was checked to determine if the float problems observed with the Stratos skid would also occur in this unit. The top of the separator was removed and 180°F OC-11 oil was poured into the separator. The floats became buoyant when the oil level was approximately 3/8 to 1/2 in. from the top of the floats. This performance was considered satisfactory and it was decided not to install retainers and springs as had been done on the same unit on the Stratos skid.

2.3 Instruments and Controls

a. SAM System: The final design of the replacement Site Area Monitor (SAM) system was completed and fabrication was 90% completed. The chambers were tested to determine the drift associated with temperature and supply voltage variations. All chambers satisfied the specification (maximum ± 15% drift over the temperature range from -65 to + 125°F (see Table 2-2). The drift associated with variation in supply voltage was acceptable (Table 2-3). Fabrication and installation of this system is scheduled for completion in January.

TABLE 2-2 - VARIATION OF SAM READING WITH TEMPERATURE

	Chamber TA-6A1000R Reading, R/hr			Chamber TA-6A100R Reading, mr/hr			Chamber TA-6A10R Reading, mr/hr		
Temperature °F	High Range	Low Range	Zero	High Range	Low Range	Zero	High Range	Low Range	Zero
-65	8	1.2	0	1100	130	0	1100	40	0
-25	7	1.2	0	1300	130	0	1200	40	0
0	7	1.2	0	1400	125	0	1200	40	0
25	7	1.2	0	1500	125	0	1100	40	0
50	7.5	1.2	0	1500	125	0	1100	40	0
75	8.0	1.2	0	1500	125	0	1100	40	0
100	8.5	1.2	0	1500	125	0	1100	40	0
125	9.5	1.3	0	1500	125	0	1100	42	0

TABLE 2-3 - VARIATION OF SAM READING WITH SUPPLY VOLTAGE
(TA-6A10R Chamber)

Supply Voltage, volts	Chamber Reading mr/hr
24	1.3
23	1.2
22.5	1.2
22	1.15
21	1.1
20	1.1

b. Start-Up Power Level Scram System: Several concepts for a power level scram system to prevent excessive fuel element hot spot temperatures during reduced coolant flow were defined in preliminary studies. The results of this work were published (Ref. 1) with the recommendation that additional studies be funded to design and develop a satisfactory system.

c. Control Cab Modification: A replacement wiring harness for the nuclear instruments was fabricated for future installation. The harness was fabricated to prevent an extended plant shutdown if the existing harness, now brittle and difficult to work with, should fail completely.

The design of a modification to the annunciator central power supply and alarm circuitry was initiated.

2.4 Fuel Elements

a. Thermal and Neutronic Analysis: Measurements of the reactivity inventory of the ML-1 reactor in both the flooded and dry condition following the pressure vessel repair program indicated an apparent decrease of approximately 0.5% ΔK/K. Analysis showed that the primary cause of the variation was the increased quantity of silver on the new reactivity shims (Ref. 2). A revised reactivity shim pattern was specified for the ML-1 to increase the reactivity of the cold, clean system to approximately 1.65% ΔK/K. The new pattern includes 19 heavy silver shims, 3 light silver shims and 39 stainless steel shims (the original pattern consists of 25 heavy silver shims, 3 light silver shims and 33 stainless steel shims).*

The reactivity shim pattern required after the projected mid-life reshimming of the ML-1-I core (after approximately 5,000 hours of full power operation) was specified. According to the present calculations, the new shim pattern will consist of 7 heavy silver shims, 8 light silver shims and 46 stainless steel shims.

*A "heavy" silver shim is plated with 67.8 gm of silver; a "light" silver shim is plated with 23.7 gm of silver.

The problem of calculating the excess reactivity of the ML-1 core in the dry condition on the basis of data developed with the core in the flooded condition was analyzed. The following conclusions were developed:

- A direct proportionality does not necessarily exist between the excess reactivity of a dry ML-1 core and the excess reactivity of the same core when flooded.

- Two different core loadings which have the same critical control rod configuration when flooded will probably not have the same reactivity when dry.

- The reactivity change in a dry core caused by an alteration in the amount of material in one location would be proportional to the reactivity change in a flooded core caused by the same alteration. However, the proportionality constant would change for different materials and no proportionality exists in the event of changes in two or more materials at the same time.

Analyses were performed in support of the program for the replacement of the ML-1-I fuel element lower spider (see 2.4e below). Calculations were made to determine the consequence of operation with the failed spiders and with various replacement spider designs (Ref. 3). It was determined, in general, that if three upper pin welds failed in an element with the smallest sized orifice (0.73 in.), the flow reduction resulting from the pins dropping into the orifice will cause the pin surface temperature to exceed 2000°F. The consequence of the lower assembly dropping as the result of an outer liner-nose guide weld failure was analyzed and slotting the lower nose guide was recommended. Calculations of the effect of this design change on the temperatures in the lower tube sheet showed that the lower tube sheet temperature would increase from about 400 to about 420°F and that the retainer ring temperature would increase from 1080 to 1160°F (Ref. 4). This temperature increase will not create a problem in the ML-1 lower tube sheet.

Experimentally determined correlations for friction factor and orifice coefficients for the modified ML-1-I fuel elements were combined with the estimated power distributions for the reshimmed core to specify the new orifice pattern (Ref. 5).

The ANASIM code (a digital computer code which simulates an analog computer) was revised to provide simultaneous print-out of 27 channels. The power generation curve used in ML-1 coolant loss analysis was revised to include the current estimates of minimum control rod shutdown worths. A problem simulating an ML-1 coolant loss accident was run using the initial temperatures based on a HECTIC code determination of nominal steady state temperature at the point of maximum power generation. The power input data simulated one year of operation at full power, with all of the beta energy and one-third of the gamma energy deposited in the fuel, followed by a shutdown resulting from a 3% $\Delta K/K$ step reduction in reactivity. The results of the run indicate that the cladding temperature of the inner ring of pins (containing UO_2 pellets) would peak eight seconds after shutdown at 1790°F. The temperatures of the cladding of the outer ring of pins (containing BeO-UO_2 pellets) would peak 64 seconds after shutdown at 1710°F.

b. IB-8T-1 In-Pile Test: Additional chemical analyses were performed on particles extracted from the IB-8T-1 (6400 hr test) cladding samples to establish whether nitrogen was present in these particles and whether the oxygen had been introduced in the previous analysis by the extraction procedures used. Previous analyses (Ref. a, pp 16) had indicated high nitrogen and oxygen content. The results of the new analyses, by vacuum fusion and Kjehldahl techniques, are shown in Table 2-4.

TABLE 2-4 - CHEMICAL ANALYSES OF IB-8T-1 CLADDING (PIN 13)

(Performed by Battelle Memorial Institute)

	Vacuum Fusion Analysis (wt%)			Kjehldahl Analysis (wt%)
Sample No.	Oxygen	Nitrogen	Hydrogen	Nitrogen
1	0.904	0.0630	0.01	0.02
2	0.480	0.0338	0.007	0.02

These data indicate much less nitrogen than was reported in the previous samples. It was determined that the section of pin 13 analyzed by BMI was from the very top (cold) part of the pin and did not contain the pi-phase which had caused embrittlement of the hottest portion of the cladding. Additional samples will be analyzed.

Metallographic samples of the IB-8T-1 element were evaluated in the AGN hot cell. Emphasis was placed on samples from pin 10 to obtain 6400-hr data from fuel pin cladding specimens which did not contain the pi-phase. Previous work emphasized the cause of fuel pin cladding embrittlement. The structure of pin 10 cladding at 7.7 in. from the bottom (X/L = 0.75) closely resembles that observed in Hastelloy X specimens after long-term normal aging (Fig 2-7).

c. IB-8T-2 In-Pile Test: Tensile tests were performed on IB-8T-2 (10,000 hr test) cladding specimens at 1200 and 1400°F to supplement earlier room temperature tensile tests (p. 18, Ref. a). Although the specimens broke in the grips, the data reveal the anticipated trends (Table 2-5).

Extensions were welded to the tubing specimens to perform the elevated temperature tensile tests. A similar procedure had been used successfully with the IB-8T-1 tubing. In the present tests, however, all specimens failed at the weld, rendering the elongation data invalid. Photmacrographs and photomicrographs were prepared of fracture zones but have not yet been analyzed.

The following general conclusions can be drawn from the data presented in Table 2-5:

- Based upon results of IB-8T-2 tensile tests, the Hastelloy X tubing material retains sufficient strength and ductility for use in a 10,000 hr ML-1 fuel element.

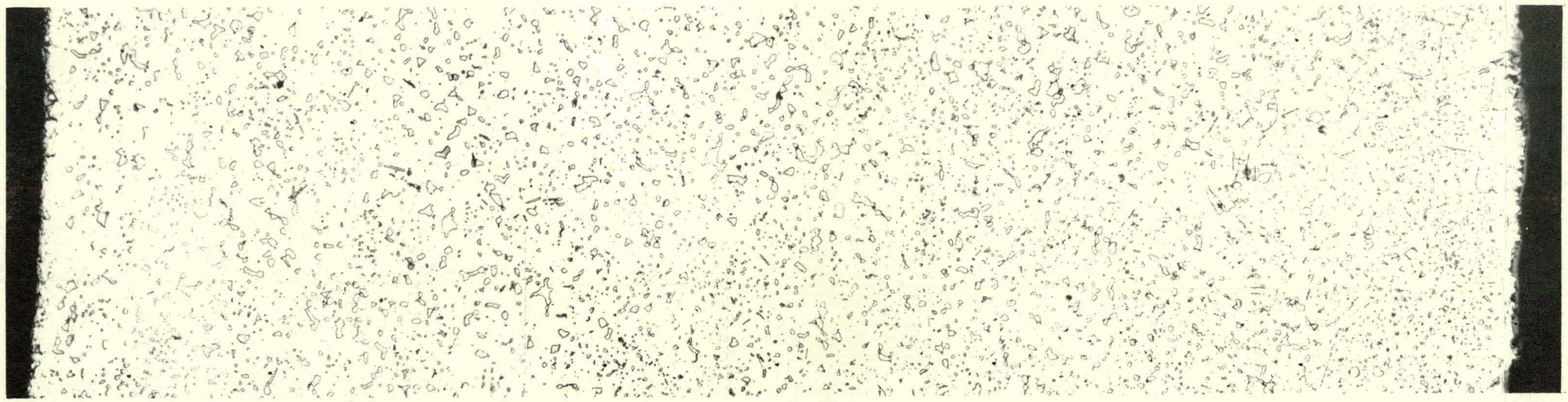


FIGURE 2-7. IB-8T-1 PIN 10 CLADDING MICROSTRUCTURE AT 7.7 INCHES FROM BOTTOM

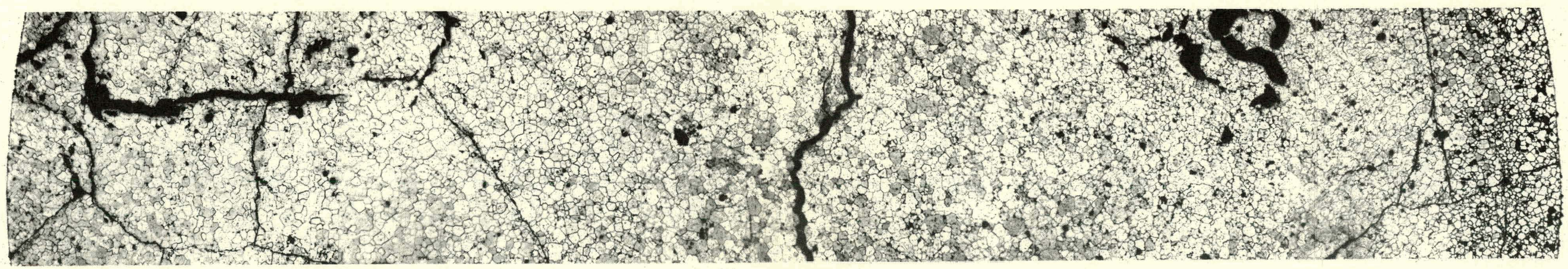


FIGURE 2-8. IB-8T-2 PIN 13 UO_2 FUEL STRUCTURE AT 7.7 INCHES FROM BOTTOM

TABLE 2-5 - MECHANICAL PROPERTY DATA IB-8T-2 FUEL PIN CLADDING

Specimen	IB-8T-2 Pin No.	Operating Temp, °F	Testing Temp, °F	Neutron Dose nvt x 10^{20}	Ultimate Tensile Strength, psi	0.2% Yield Strength psi	Total Elong., %	Location of Fracture
B-102	--	Unexposed	Room	--	114,800	*	37.5	Middle of gage section
B-102	--	Unexposed	1200	--	67,000	36,800	40.7	--
T-1	1	1455	Room	10.0	67,000	41,900	8.3	
T-3	12	1375	Room	9.8	86,300	48,800	**	In gage section
T-6	7	1195	Room	6.5	116,300	59,400	14.5	End of center pin
T-7	12	1480	Room	10.1	98,400	53,600	9.1	
T-9	15	1300	Room	6.3	95,070	46,700	17.8	Center of gage section
T-10	16	1300	Room	6.3	101,000	46,100	28.8	In gage section 1 in. from center pin
T-11	16	1200	Room	7.3	107,700	49,700	17.4	In gage section
T-12	18	1480	Room	10.6	94,490	44,400	15.5	In gage section
T-2	6	1195	1200	6.0	39,000	29,600	6.1*	Broke in grips
T-5	7	1240	1200	5.5	54,100	31,900	9.2*	Broke in grips
T-4	18	1385	1400	10.2	34,900	27,600	3.1*	Broke in grips
T-8	13	1385	1400	11.0	35,500	27,800	11.0*	Broke in grips

* Data invalid

**Specimen slipped in grips, elongation data suspect.

- Significant reductions in strength and ductility occurred in some specimens although no correlation with neutron dose is apparent. (Some of the specimens were not completely straight; this factor can be expected to have influenced the results.)

A summary report of the investigation of the mechanical properties of the IB-8T-2 test element was published (Ref. 6).

Metallographic specimens of the IB-8T-2 fuel, cladding and inner liner were evaluated at the AGN hot cell. The UO_2 fuel in the inner ring of fuel pins displayed more extensive cracking than was noted in the $BeO-UO_2$ fuel in the outer ring of fuel pins. Because of the higher relative burnup at the fuel pellet edges, fuel in this area was greatly embrittled, as evidenced by massive pull-out during metallographic preparation. This can be seen at the right edge of Fig 2-8, which was polished and etched at the AGN hot cell. The IB-8T-2 cladding examination confirmed that no embrittlement or other deleterious condition existed in the cladding. Selective etching techniques were used in examination of the inner liner to demonstrate that the condition observed on this component was not due to a carbide formation.

d. <u>Metallurgical Support of the ML-1-I Air Cycle Operation:</u> Low-cobalt Hastelloy X tubing, identical with that used in the ML-1-I fuel elements, is being exposed to high temperature air to determine the amount of oxidation attack and corrosion product loss to be expected by conversion of the ML-1 power plant to an air cycle. Mechanical property changes associated with aging during high temperature air exposures are also being evaluated.

During the report period, Hastelloy X tubing specimens were examined after 2500-hr exposure to air at high temperatures. The appearance of the outside surface of the specimens (Fig 2-9) reveals that the depth of oxide penetration and surface film formation increases as a function of temperature with the maximum penetration, averaging 0.0035 in., occurring on the specimen exposed at 1800°F. Average measured depths of oxide penetration into the low cobalt Hastelloy X tubing is given in Table 2-6.

TABLE 2-6 - AVERAGE MAXIMUM DEPTHS OF OXIDE PENETRATION IN LOW-COBALT HASTELLOY X TUBING IN AIR

Exposure Temperatures °F	Penetration, in. 1000-hr Exposure	2500-hr Exposure
1300	0.0000	0.0004
1450	0.0002	0.0012
1600	0.0006	0.0019
1250	0.0018	0.0028
1800	0.0020	0.0035

Evaluation of the rate of oxide growth and spalling on Hastelloy X tubing in air at 1750°F by measuring the change of weight of specimens continued. The continuous weighing balance was modified to permit the recording of the weight change of multiple samples. A short term (160-hr) test was successfully completed and a long duration (10,000-hr) test was initiated.

The results of the short term (160-hr) test are plotted in Fig 2-10. A Powerstat malfunction during this shake-down test resulted in rapid cooling of the test specimens and the oxide scale spalling and the discontinuity apparent in Fig 2-10. It can be seen that data from three of the samples agreed quite closely while the fourth sample showed much larger weight gains. This appears to be the result of gross oxidation on the inside surface of the sample. Weight gains of the other samples was initially rapid as a surface oxide film formed; after the formation of this protective film, the rate of weight gain decreased.

Tensile tests were performed on ML-1-I tubing samples exposed at various temperatures for 1000 hours; the results are given in Table 2-7. The exposure of samples will continue until 10,000-hr data is obtained.

TABLE 2-7 - ROOM TEMPERATURE TENSILE PROPERTIES OF LOW-COBALT HASTELLOY X TUBING

Tube No.	Temperature Location, °F	Exposure Time, hr	Ultimate Tensile Strength, psi	Yield Strength, psi	Elongation %
A2284	Unexposed	1000	106,100	*	42.6
A2370	Unexposed	1000	107,100	*	41.2
A2385	1300	1000	122,000	62,500	16.7
A2385	1450	1000	109,761	50,000	29.6
A2385	1750	1000	108,571	47,600	34.1
A2385	1750	1000	103,571	47,600	37.0
A2070	1300	1000	119,523	58,300	17.2
A2070	1450	1000	109,047	51,200	27.6
A2070	1750	1000	100,238	44,300	21.3
A2070	1750	1000	100,952	42,800	39.7

*Data invalid

e. Modification of ML-1-I Fuel Element: The ML-1-I fuel element inspection performed in October 1963 (Ref. 7) revealed five elements with broken and/or deformed lower spiders and heavy corrosion on the lower spiders of all elements. Fig 2-11 shows the lower spider from fuel element Serial No. 11 (S/N 11); the type of break and the massive corrosion are typical of other elements examined.

A detailed hot cell examination and a material analysis were performed for fuel element S/N 11 to determine the cause of the spider failures (Ref. 8). Hot cell metallographic examination of the lower spider disclosed extensive areas of shrinkage porosity, cracking through areas where off-center drilling reduced the wall thickness and some intergranular corrosion. No indication of carbide precipitation was observed and the material appeared to be ductile. It was also noted that the fuel pin tips were stuck in the guide holes of the lower spider. Details of the metallurgical evaluation are presented in Section 2.4f.

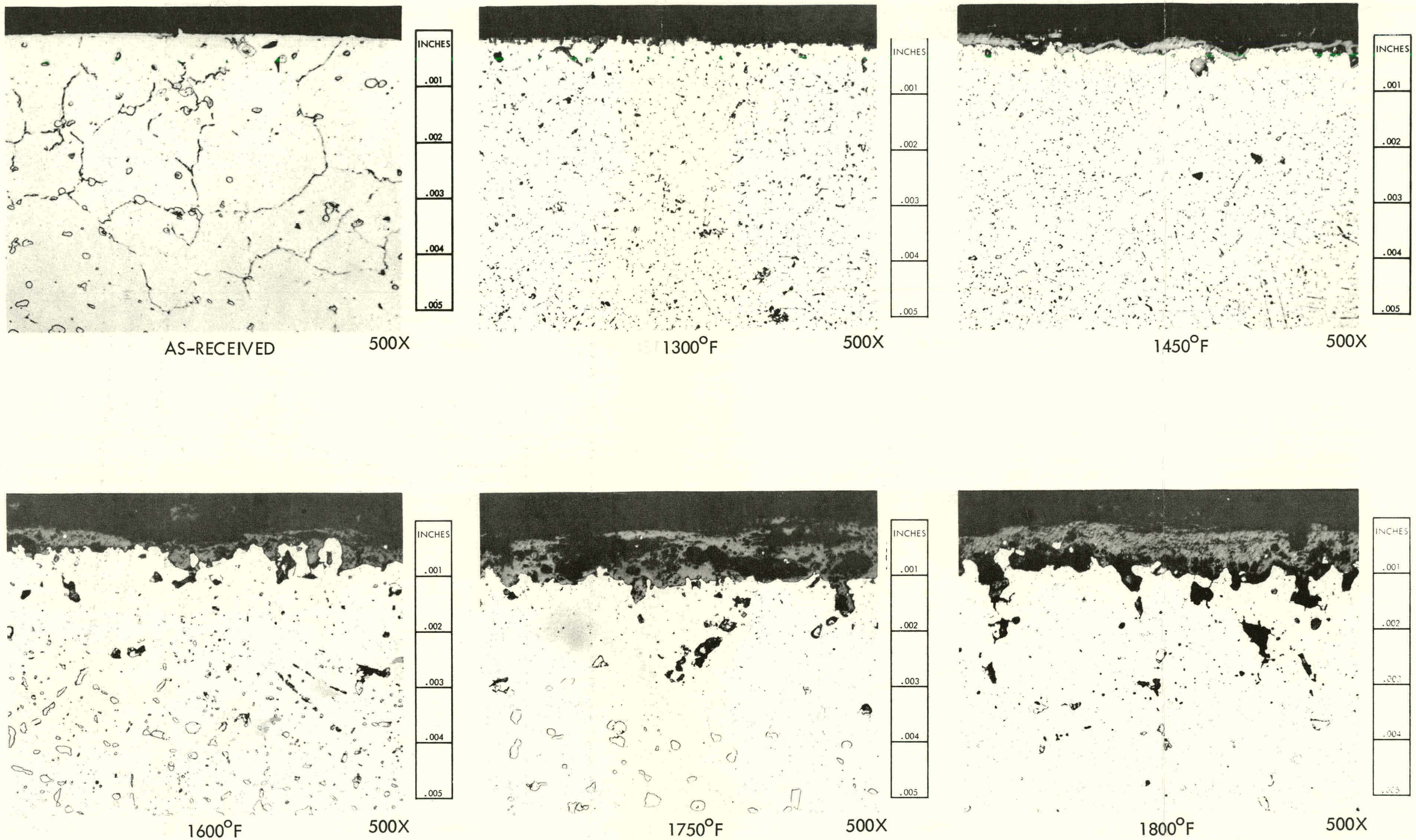


FIGURE 2-9. ML-1-I HASTELLOY X TUBING EXPOSED TO AIR FOR 2500 HOURS OUT-OF-PILE

11.2-64-288

11.2-64-289

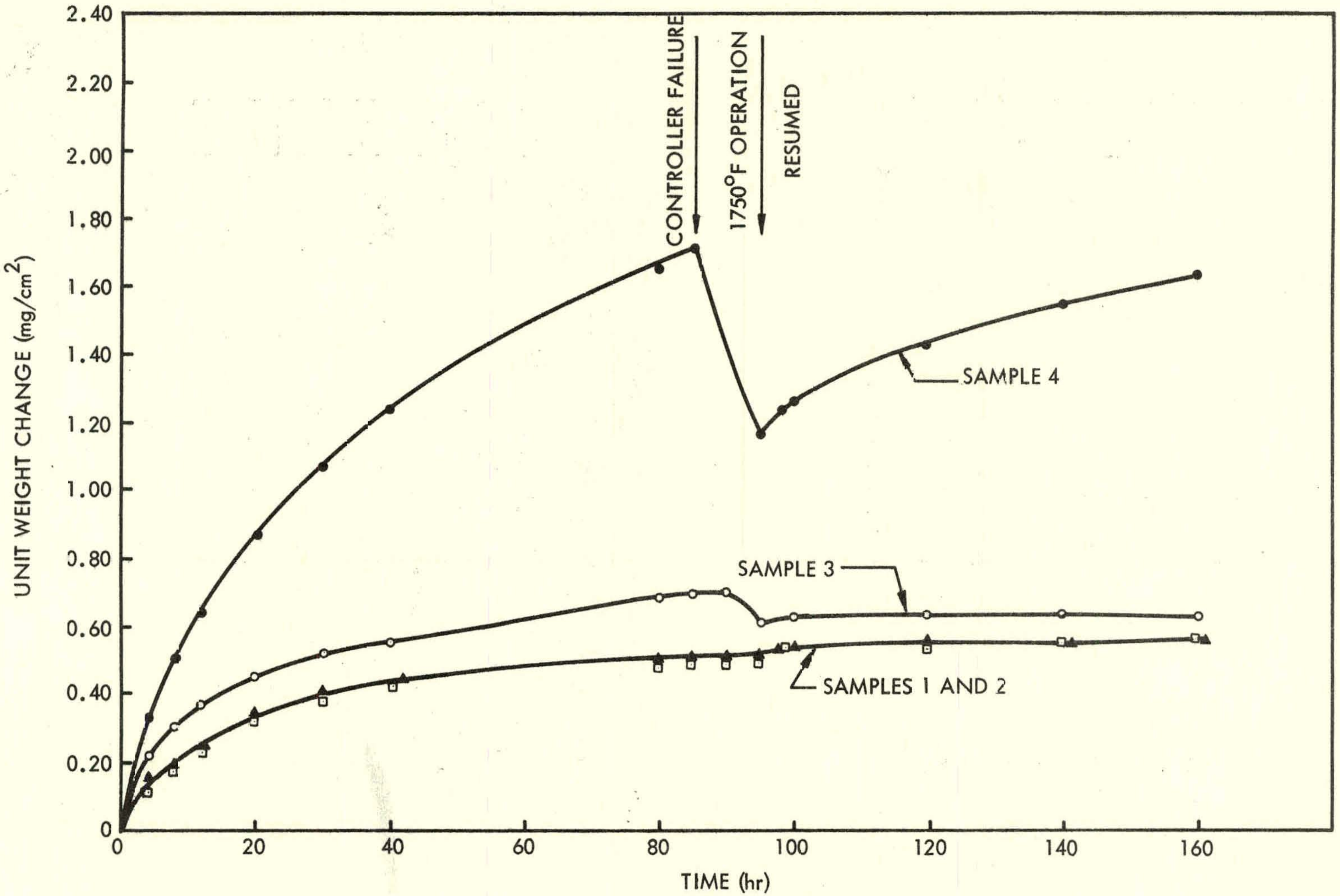


FIGURE 2-10. CHANGE OF WEIGHT OF ML-1-I HASTELLOY X TUBING SAMPLES AS A FUNCTION OF TIME IN AIR AT 1750°F

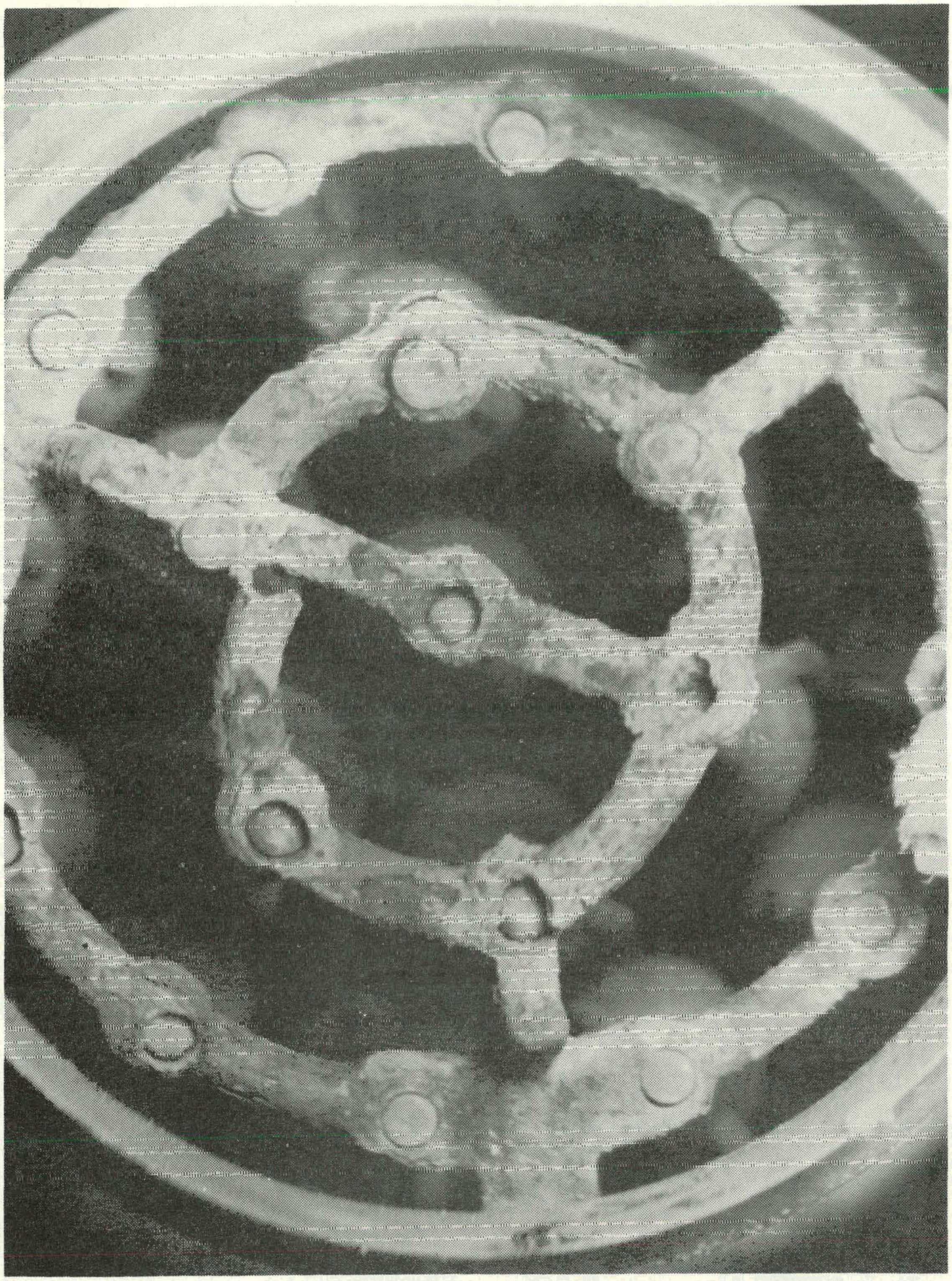


FIGURE 2-11. ML-1-I, S/N 11 LOWER SPIDER (SHOWING LATERAL DEFORMATION OF INNER RING AND MASSIVE CORROSION)

11.2-64-290

Chemical analyses of the lower spider material were performed. One serious deficiency was observed; the chromium content was 11.01% instead of the specified 16-18%. This deficiency accounts for the heavy corrosion noted on the lower spiders since a minimum of 12% chromium is required to passivate iron in water.

It was concluded that the failures were caused by corrosion of the low-chromium material and stresses aggravated by loss of effective area through porosity and imperfect fabrication.

Even though the upper spider was fabricated to the same material specifications, hot cell examination revealed only slight corrosion (Fig 2-12). Chemical analysis of one sample indicated that the chromium content (14.87%) was sufficiently high to passivate the part. Differences in design between the upper and lower spiders eliminate the possibility of aggravated stresses due to imperfect machining. It was concluded that the upper spider is adequate and should satisfactorily achieve the 10,000-hr design lifetime.

As a result of the failure investigation, the following corrective actions were considered:

1) Operate the core without modification or repair

2) Remove the lower spiders and operate without any secondary structure

3) Remove the lower spiders and install a screen between the pin tips and the orifice in each element

4) Install a replacement spider fabricated from Hastelloy X bar stock which will support the six inner ring pins in each element

5) Install a replacement spider fabricated from Hastelloy X bar stock which will support the 18 fueled pins in each element

6) Install a new insulation package with an improved, 19-hole cast spider in each element

7) Provide a new core loading.

These alternatives are listed in order of increasing complexity, cost and time required for implementation. Alternatives 1, 2 and 3 were eliminated because they did not satisfy the design or safety criteria of the original fuel element. Alternatives 6 and 7 were not considered because of excessive cost and the time required for implementation.

The design and testing effort was concentrated on the development of a 6-hole lower spider (alternative 4 above) since a preliminary evaluation showed this concept apparently involved minimum cost and least perturbation to the schedule. A design for such a spider was developed and tests revealed that the modified spider was technically adequate and would satisfy the original design and safety criteria, even in the unlikely event of an upper spider failure (Ref. 9). However, in discussions with the USAEC, it was decided that sufficient justification existed to warrant the expenditure of additional funds

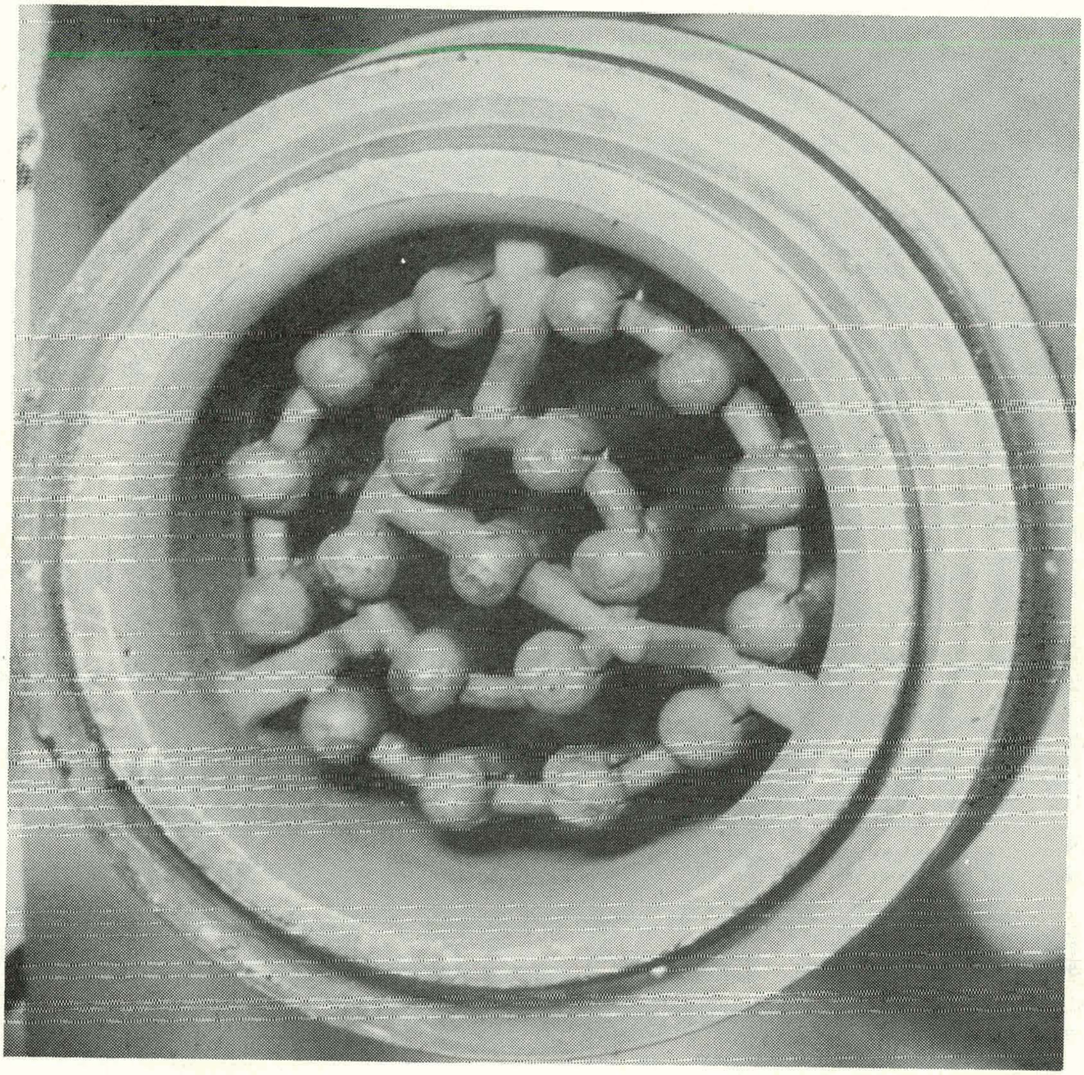


FIGURE 2-12. ML-1-I, S/N 11 UPPER SPIDER (BEFORE DISASSEMBLY)

11.2-64-291

to develop an 18-hole spider (alternative 5 above) because of the additional reliability of such a design.

Several concepts were evaluated for the 18-hole spider design and, after extensive flow testing, a final configuration was selected (Fig 2-13). A flow test was conducted to determine the pressure drop characteristics of the new design. The results of this work are shown in Fig 2-14. The correlation between flow control orifice coefficient and orifice diameter (Fig 2-15) was defined for the new design (Ref 10) and orifice sizes specified to adjust the flow rates in the core.

The 18-hole design does not prevent the center pin from dropping if it becomes detached from the upper spider. However, calculations based on flow tests indicate that such a failure would cause an increase in pin surface temperature of only 22 F^o for the worst case. Calculations indicate that a dropped center (unfueled) pin would not cause any significant change in core reactivity.

During the inspection of the fuel element S/N 11, it was observed that a heat affected zone existed at the outer liner-nose guide weld (see Section 2.4g). This condition prompted consideration of the effect of a weld failure that would permit the lower assembly to fall on top of the reactor baffle surface. Calculations indicated that such an event would cause an essentially total coolant flow blockage and that fuel pin cladding temperatures in the failed element would be intolerably high. As a consequence, the design of the orifice assembly (which was removed to permit installation of the modified spider) was altered to provide slots in the lower surface. Calculations based on flow tests showed that a weld failure would decrease flow in the modified element but that the fuel pin surface temperature would increase only 38 F^o in the worst case.

Mechanical bending tests, performed to compare the relative strength of the modified and original spiders, showed that the new spider was plastically deformed 0.134 in. under a load of 750 lb on the outer ring at room temperature. By comparison, the original spider deformed 0.140 in. under a load of 165 lb.

Tests were performed, using the brittle coating method, to evaluate the stresses in the region of the upper spider due to flow-induced vibrations with no lower spider installed in the test element. These tests showed that the upper spider was not subjected to any stresses greater than 15,000 psi. Since the endurance limit of stainless steel at 800°F is greater than 15,000 psi, fatigue failure of the upper spider is not anticipated.

A summary report of the development of the 18-hole spider was published (Ref. 11).

After the suitability of the modified spider design was demonstrated, a procedure was developed for installing the new part in the existing ML-1-I fuel elements. The first step in the procedure was the removal of the orifice assembly (see Fig 2-16) by drilling out the three dowel pins which connected this assembly to the nose guide. This step was followed by removal of the lower spider by electric arc decomposition (ELOX process). The next step in the

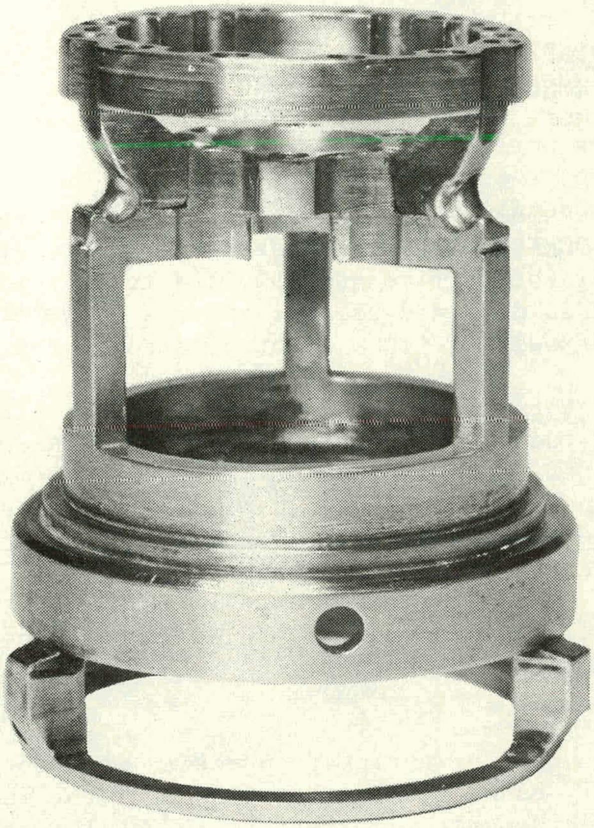


FIGURE 2-13. MODIFIED ML-1-I LOWER SPIDER

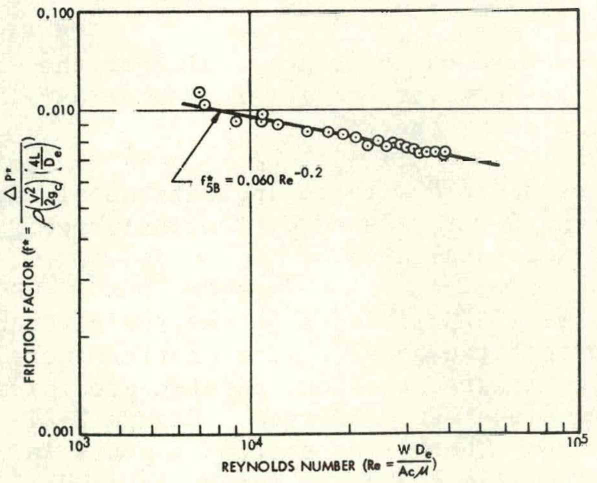


FIGURE 2-14. CORRELATION OF FRICTION FACTOR VERSUS REYNOLDS NUMBER FOR ML-1-I WITH 18 HOLE SPIDER AND SMOOTH BORE ORIFICE

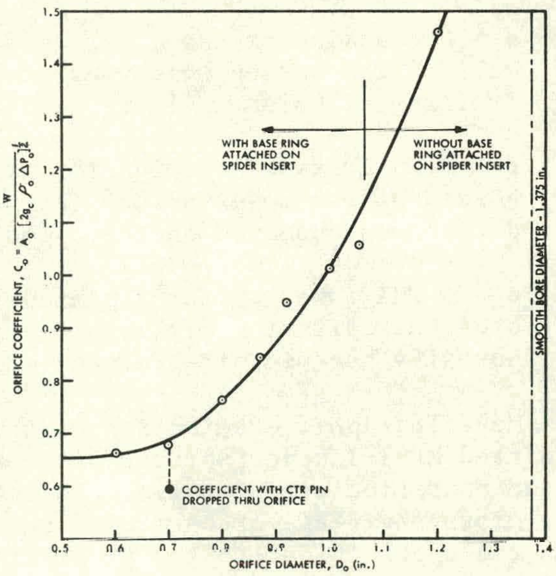


FIGURE 2-15. CORRELATION OF ORIFICE COEFFICIENT VERSUS ORIFICE DIAMETER FOR ML-1-I WITH 18 HOLE SPIDER

operation was the installation of the modified spider, and orifice assembly, which involved the engagement of the 18 pin tips in the holes in the spider. The three dowel pins were then inserted through the nose guide and welded in place.

At the end of December, jigs and fixtures for the modification of the fuel elements in the GCRE pool had been designed and fabricated and the procedures for removal of the orifice assembly and lower spider had been demonstrated. Replacement spiders and orifice assemblies were being fabricated and completion of the modification was anticipated by the end of January 1964.

f. Metallurgical Evaluation of ML-1 Spiders: As a result of difficulties with the ML-1-I lower spiders (discussed above), a metallurgical evaluation of both the upper and lower spiders of an ML-1 element was made. Samples from ML-1-I S/N 11 lower and upper spiders and samples from spare, unirradiated spiders were evaluated. In addition, chemical analyses of the ML-1-I S/N 11 and unirradiated spider castings were performed.

The ML-1-I S/N 11 lower spider was found to be of poor metallurgical quality. Hot cell examination (Ref. 12) of metallographic sections disclosed the following:

- Extensive areas of shrinkage porosity were observed (Fig 2-17), that were usually connected to the surface or the inside diameter of the drill holes as evidenced by corrosion product formation on the surfaces of the cavities. Figure 2-18 is typical of this condition. The most porous areas occurred in the inner ring near the legs connecting to the outer ring.

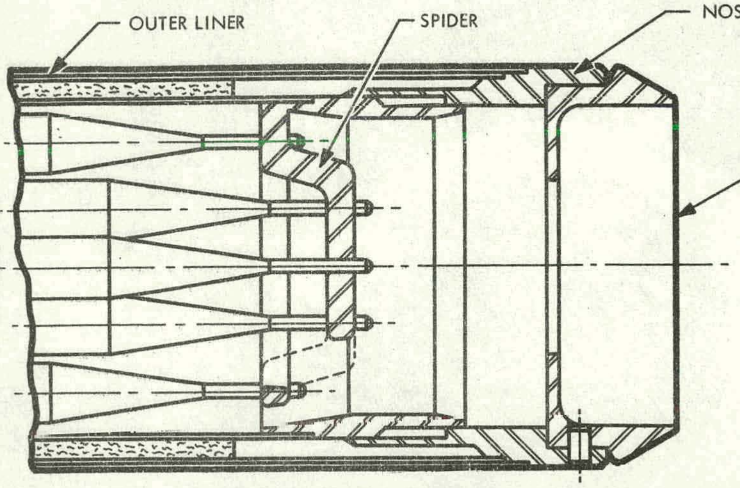
- Cracking of the lower spider occurred through areas of the side wall of the bosses which were thinner than normal because of off-center drilling of holes. Figure 2-19 is typical of this condition.

- Intergranular oxidation was observed on the cracked side of the bosses of the lower spider (Fig 2-20). However, no intergranular oxidation was observed on the opposite side of the bosses.

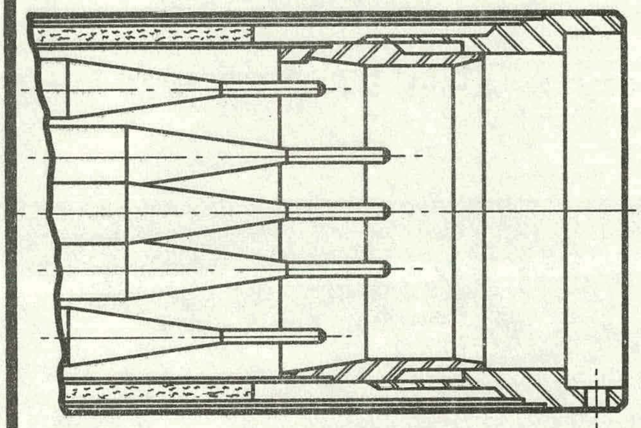
- The lower spider appeared to be ductile with no indications of carbide precipitation although the strength was reduced by the shrinkage porosity because of a reduced cross sectional area.

Metallographic examination of the ML-1-I S/N 11 upper spider and five unirradiated ML-1-I upper spiders disclosed that the upper spider castings were in good condition structurally. No intergranular corrosion, carbide precipitation, gross shrinkage or harmful slag inclusions were observed. Figure 2-21 shows the upper spider-to-top plug weld zone. The spider section appears in excellent condition and the bond between the weld and upper spider is good. Some shrinkage has occurred at the top of the weld zone in the Hastelloy X filler material but the depth is not extensive enough to cause concern. Figure 2-22 shows the center microstructure of the upper spider.

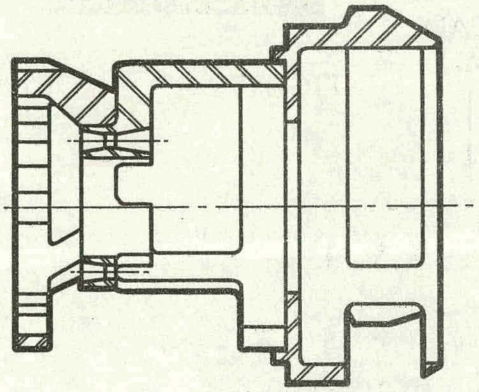
The results of the chemical analyses performed on ML-1-I S/N 11 and other spider samples are given in Table 2-8. In the analyses, duplicate samples of



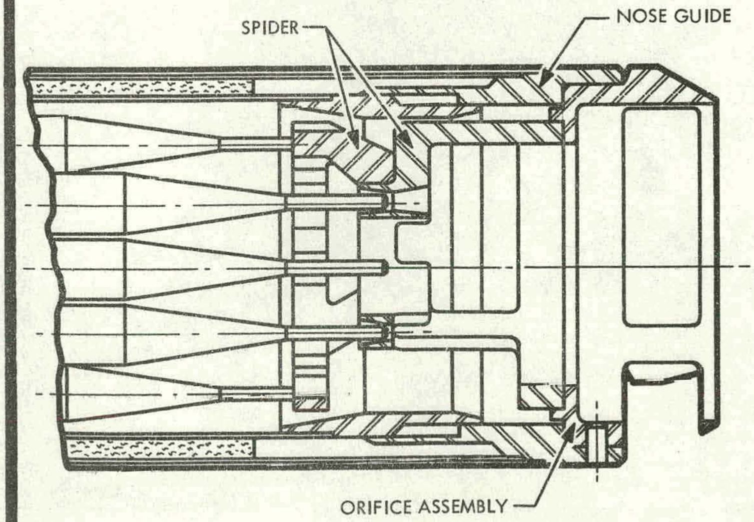
ORIGINAL CONFIGURATION

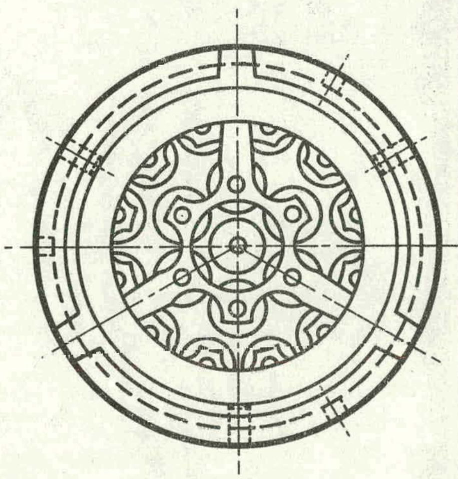


CONFIGURATION AFTER REMOVAL OF SPIDER AND NOSE GUIDE



CONFIGURATION OF MODIFIED SPIDER AND ORIFICE ASSEMBLY





CONFIGURATION FOLLOWING REASSEMBLY

FIGURE 2-16. ML-1-I FUEL ELEMENT MODIFICATION

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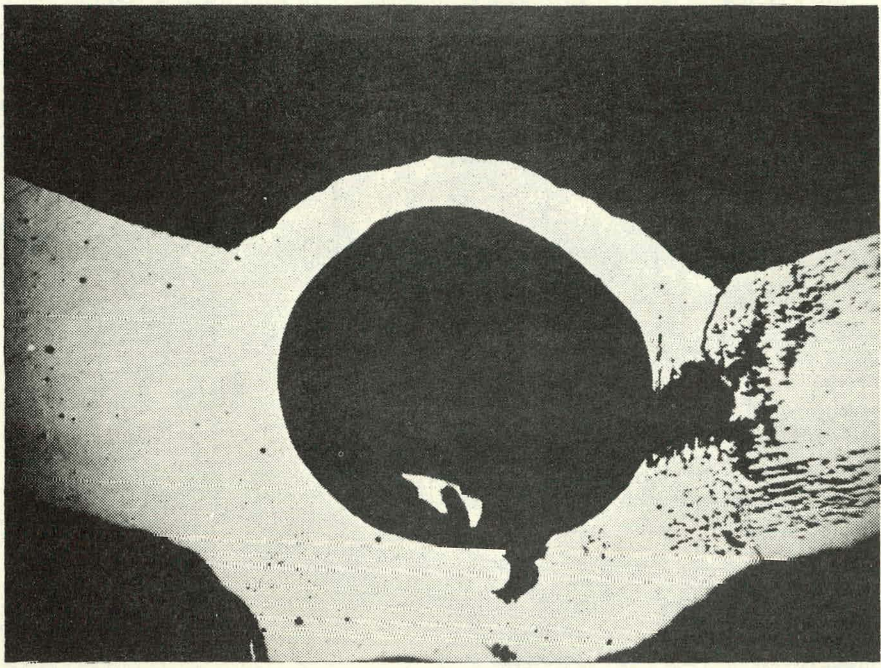

FIGURE 2-17. CROSS SECTION OF ML-1-I, S/N 11 LOWER SPIDER NEAR HOLE FOR PIN 16

FIGURE 2-18. OXIDE PRODUCT FORMATION ON SHRINKAGE CAVITY OF ML-1-I, S/N 11 LOWER SPIDER NEAR HOLE FOR PIN 16

0.04
0.02
0
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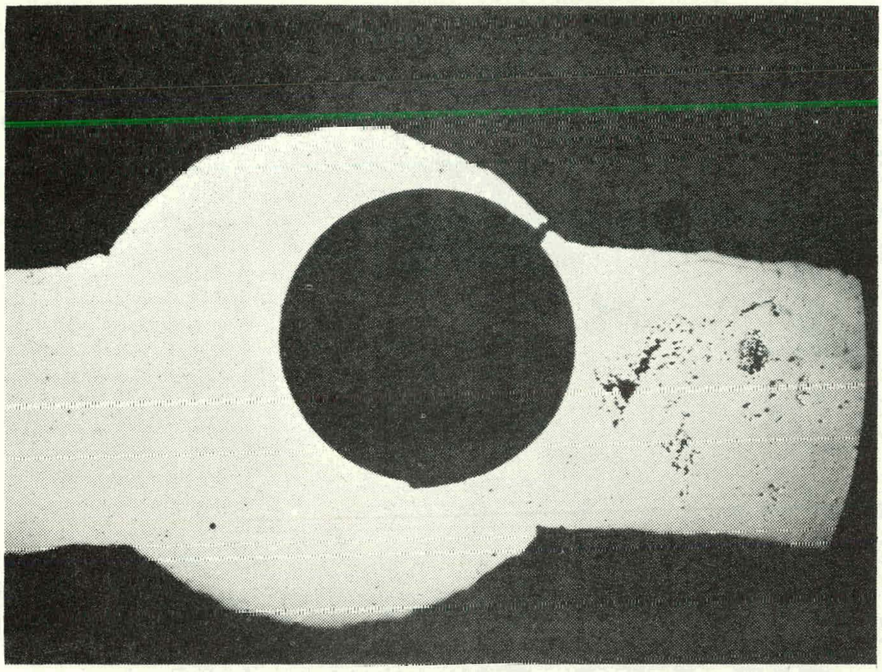

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FIGURE 2-19. CRACK IN ML-1-I, S/N 11 LOWER SPIDER SECTION

0.01
0
SCALE (in.)

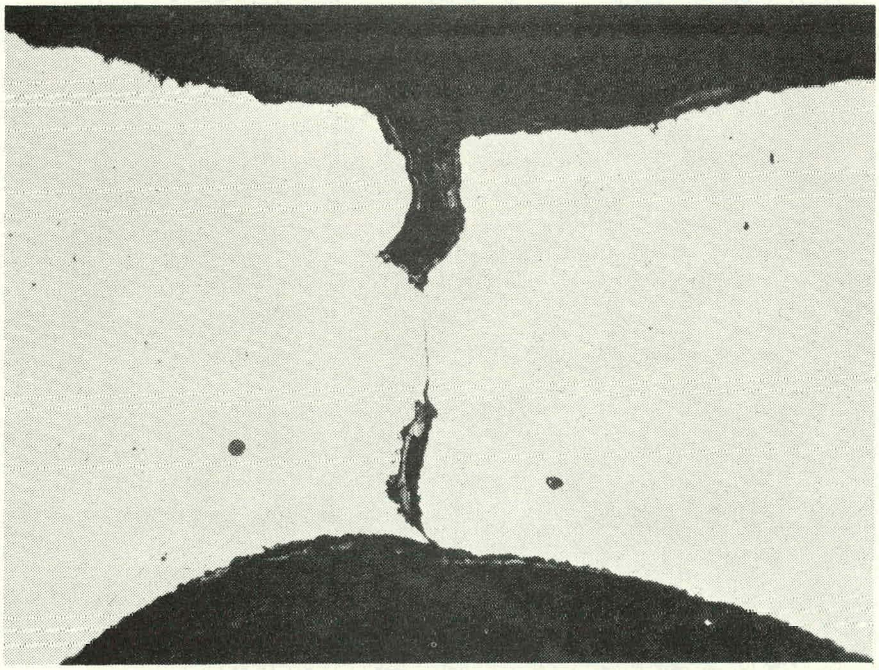

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FIGURE 2-20. INTERGRANULAR OXIDATION OF ML-1-I, S/N 11 LOWER SPIDER

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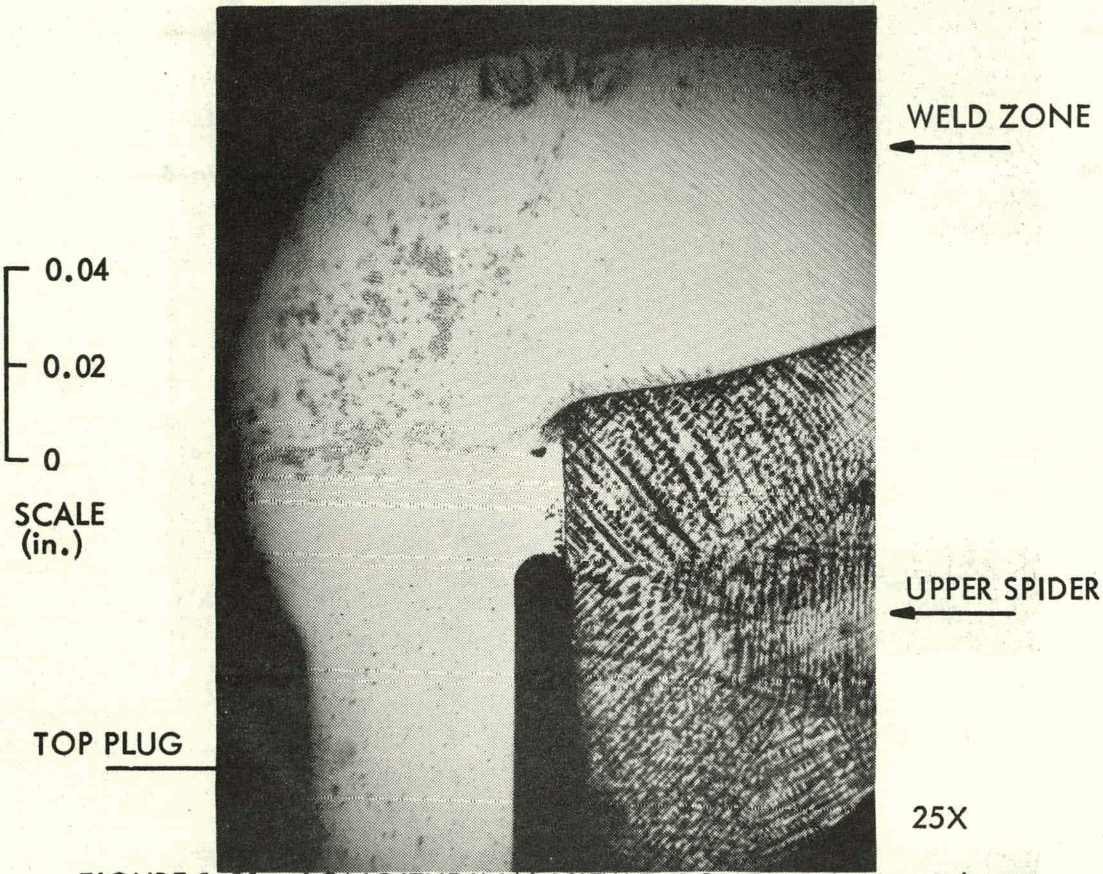

FIGURE 2-21. LONGITUDINAL VIEW THROUGH ML-1-I, S/N 11 UPPER SPIDER: TOP PLUG WELD ZONE

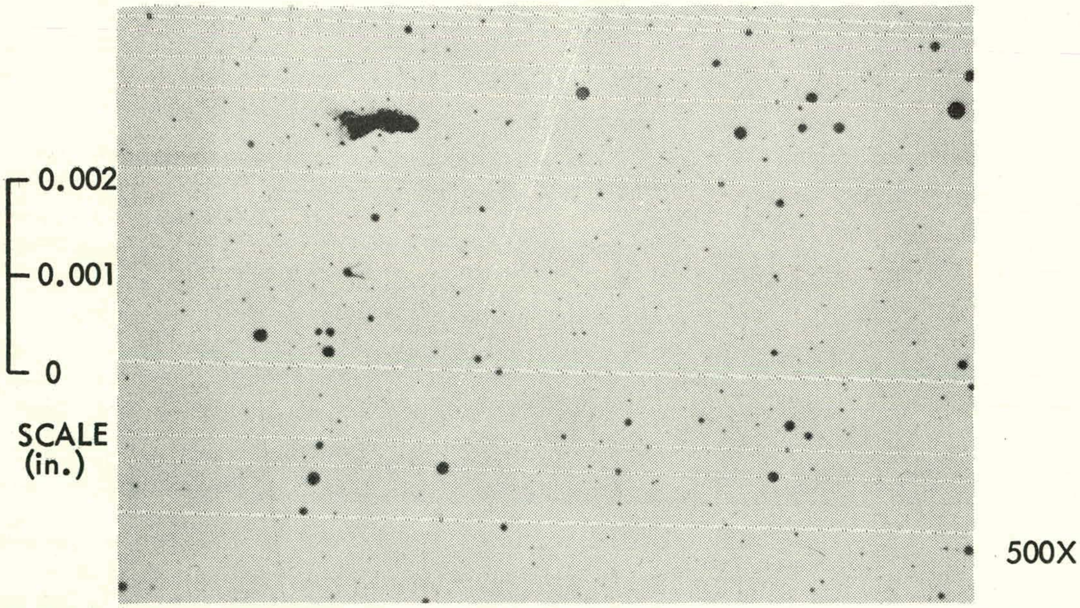

FIGURE 2-22. CENTER VIEW OF UPPER SPIDER CASTING: ML-1-I, S/N 11

TABLE 2-8 - SUMMARY OF CHEMICAL ANALYSES OF IRRADIATED AND UNIRRADIATED ML-1-I SPIDER CASTINGS

	Element %				
	Cr	Fe	Ni	Mo	C
ICPP Analyses					
ML-1-I S/N 11					
Lower Spider	11.01	69.26	13.26	2.41	0.0502*
Upper Spider	14.87	71.91	13.08	2.51	0.0355*
ML-1 Lower Spider (Sample 1)	10.96	70.50	13.82	2.28	--
Anamet Lab Analyses					
ML-1 Lower Spider (Sample 1) (for checking purposes)	11.00	Bal	13.40	1.58	--
ML-1 Upper Spider (Sample 1)	12.48	Bal	13.36	1.64	0.0175*
ML-1 Upper Spider (Sample 3)	14.72	Bal	13.43	1.69	0.0148*
ML-1 Upper Spider (Sample 4)	14.62	Bal	13.57	1.59	--
ML-1 Upper Spider (Sample 7)	14.73	Bal	13.43	1.63	0.0175*
ML-1 Upper Spider (Sample 9)	14.22	Bal	13.35	1.94	0.0221*
ML-1 Upper Spider (Sample 13)	12.77	Bal	13.63	1.79	0.0134*
Casting Vendor's Analyses					
Vendor Certification of Upper and Lower Castings	17.52	Bal	10.91	2.58	0.02*
Specification					
Specification for Type 316L Investment Castings	16-18	Bal	12-14	1.5-2.25	0.03

*Analyses for carbon performed by AGN Chemistry Department

an ML-1 lower spider analyzed by wet chemical techniques by Phillips Petroleum Co. at the Idaho Chemical Processing Plant (ICPP) at NRTS, and were analyzed at a commercial laboratory (Anamet) to check the reproducibility of analyses reported by ICPP. Six ML-1 upper spiders were also analyzed for chromium, nickel, and molybdenum at Anamet by wet chemical techniques and for carbon at AGN by conductometric methods. Evaluation of the data shown in Table 2-8 indicates that the analytical techniques used by ICPP and Anamet produce results in close agreement with the exception of the analyses for molybdenum. The ICPP analyses of the ML-1-I S/N 11 upper and lower spiders therefore appear to be reliable.

The corrosion of the lower spider was attributed to preferential galvanic corrosion. This condition occurred because the minimum of 12% chromium necessary to maintain passivate stainless steel (Ref. d) was not present. The upper spiders are also low in chromium content but above the 12% required for passivation. The carbon content of all the ML-1 upper spider samples is below 0.03% and the material is, therefore, considered immune to carbide precipitation.

Corrosion products which broke from the lower spider during disassembly were also analyzed at ICPP by X-ray diffraction. The results indicated that about 80% of the sample was a reddish colored material, Fe_2O_3; 10% was a dark reddish-brown amorphous material, and 10% was a dark unidentified substance. X-ray fluorescence analysis of the spider samples was also attempted but the patterns could not be interpreted because of inadequate standards and the rough surface of the spider castings.

A summary report of this investigation was published (Ref. 13)

g. ML-1-I S/N 11 Outer Liner Evaluation: During sectioning of ML-1-I S/N 11 fuel element (Ref. 14), an abrasive cut-off wheel was used to separate the outer liner and poison foil package from the nose guide. This sectioning produced circumferential cracks directly adjacent to the weld between the AISI Type 316 stainless steel outer liner and the stainless steel nose piece. These cracks also branched into a line connecting the spot welds between the poison foil package and the outer liner.

The weld between the outer liner and nose piece was made in the field after the initial fabrication of the fuel elements. The records indicate that nothing abnormal occurred during the welding. During reactor operation, the temperature of this portion of the outer liner and the nose piece is close to the outlet gas temperature (1200°F).

The results of evaluation of metallographic sections taken from the outer liner are:

- On the 5 o'clock side, intergranular cracking occurred in the heat affected zone of the weld between the outer liner and the nose piece (Fig 2-23).
- On the reverse (11 o'clock) side of the outer liner, no cracking occurred but carbide precipitation was observed in the heat affected zone of the weld and directly adjacent to the spot welds (Fig 2-24).
- In areas away from the heat affected zones, no carbide precipitation was observed; the structure was typical of as-fabricated austenitic stainless steel.

It was concluded that intergranular failure of the outer liner was caused by deformation during sectioning. No cracking occurred during service. The intergranular cracking was caused by carbide precipitation in zones affected by welding heat. The rest of the outer liner was unaffected by high temperature operation or long term storage in water.

2.5 System Performance Evaluation

A proposed addendum to the ML-1 Hazards Summary Report covering the recommended removal of the ultimate shutdown device was published (Ref. e).

The CHOP code was revised to include current data on component performance and to facilitate alterations of input data to permit calculations of a wide range of ML-1 and ML-1A system performance conditions. The reference system analysis and energy balance for the ML-1 power plant was updated and published (Ref. 15).

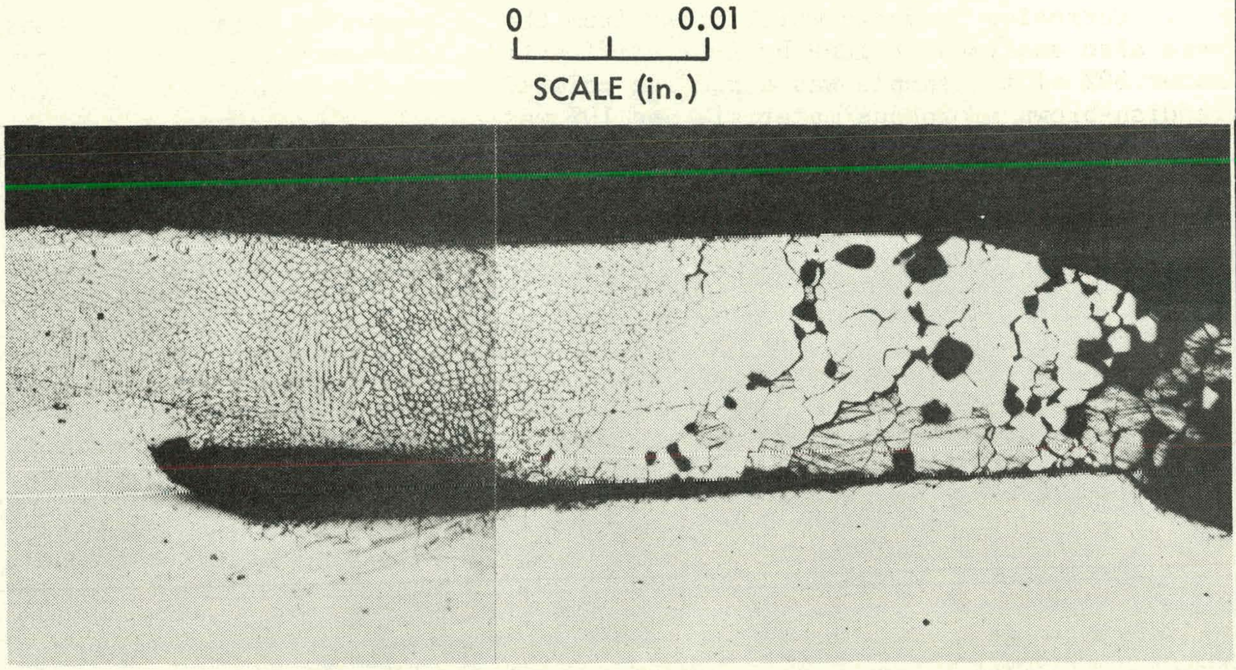

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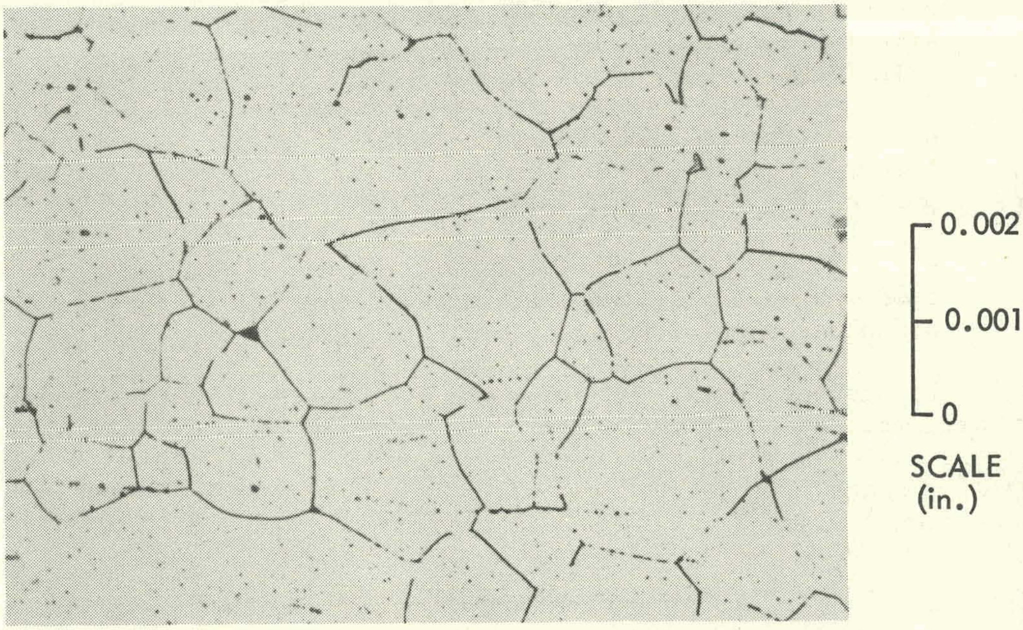
FIGURE 2-23. INTERGRANULAR SEPARATION IN ML-1, S/N 11 OUTER LINER NEAR WELD ZONE



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3.0 ML-1 DEVELOPMENT AND IMPROVEMENT

3.1 Reactor and Auxiliaries

This task was inactive during the period.

3.2 Power Conversion

a. Clark Turbine-Compressor Set (CSN-1): The modification, repair, and reassembly of the CSN-1A t-c set were completed. The unit, as assembled, included redesigned turbine discs and blades and the CSN-2 type adjustable turbine stators. In addition, the following significant additions were made to the internal instrumentation of the t-c set:

- Multiple element pressure probes at the compressor inlet, compressor discharge, and turbine inlet
- Multiple element temperature probes at the compressor inlet, compressor discharge, turbine inlet, and turbine discharge
- T-C set rotor shaft radial displacement probes, located at the compressor and turbine journal bearings
- Bearing temperature indicating thermocouples (to supplement the thermistors previously installed)

Clark Brothers submitted a reference bearing design to AGN for review. This bearing is a full fluid-film, sleeve-type bearing incorporating a step to promote an internal load by self-pumping action (Fig 3-1). Analyses by AGN and an ACN consultant indicates that this bearing will have a whirl instability threshold of 24,500 rpm and a rotor/bearing critical speed of 15,120 rpm (Ref. 16).

This bearing was installed in the CSN-1A and tested for about 18 hr, including two hours of self-sustaining operation at nearly full rotational speed in the open-cycle configuration. Lack of adequate fuel gas pressure to the test facility combustion chamber prevented reaching full-speed, full-temperature conditions. (This deficiency was corrected and subsequent tests were

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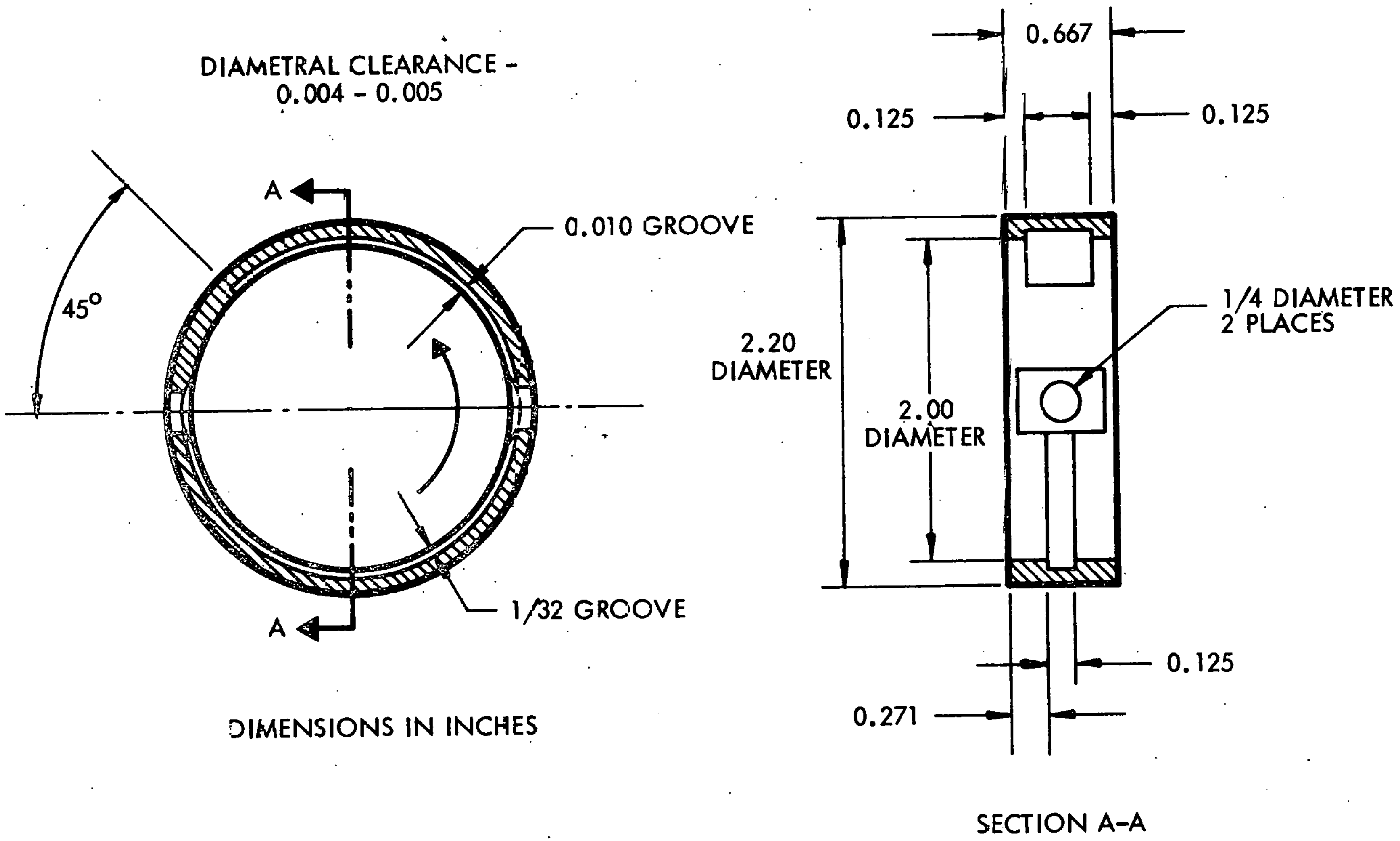

FIGURE 3-1. ANTI-WHIRL JOURNAL BEARING FOR CSN-1A

conducted at rated t-c set speed and temperature. This preliminary testing indicated that the bearing design was stable and satisfactory for normal operation.

The initial 18 hr of open-cycle testing produced little valid thermodynamic data due to problems with the facility instrumentation. The turbine inlet temperature measurement, a four-element temperature rake, indicated individual temperature readings which varied by as much as 190°. However, the preliminary data indicated that the turbine flow area was still too large.

Following these tests, the CSN-1A was removed from the test stand and both journal bearings were removed for inspection. No measurable dimensional change was found. A visual inspection revealed extremely slight circumferential scoring in the lower half of the bearings. However, this was so slight that to attempt repair would have resulted in greater damage to the bearing. Consequently, it was decided to continue to use the bearings. The facility combustion chamber configuration was altered to reduce the turbine inlet temperature stratification as indicated by the variation in temperature readings.

Tests to investigate the effects of varying bearing oil inlet temperatures and pressures on bearing/rotor stability were initiated. At t-c set rotational speeds between 18,000 and 22,000 rpm, an intermittent, low-amplitude vibration at 0.8 of rotational frequency was observed. When this vibration was evident, the t-c set case vibration increased from a nominal amplitude of 0.00005- to 0.0001-in. Investigation established that this vibration was due to backlash in the t-c set gearbox, caused by operation under the no-load conditions associated with open-cycle, self-sustaining testing. It was decided to install a device to provide a slight load on the t-c set gearbox to eliminate the backlash.

A review of the thermodynamic data from this test, with the improved turbeing inlet temperature indication, verified the preliminary analysis that the turbine flow area was still too large. It was decided to partially disassemble the CSN-1A to inspect, and possibly reset, the turbine stator angles. Disassembly inspection revealed that a radial clearance gap existed at the root of the first stage turbine stator blades, and at the tip of the second stage turbine stator blades. These gaps, measuring approximately 0.030-in. radially, permitted a significant internal bypass flow within the turbine which made it appear that the turbine flow area was too large. During this inspection, no dimensional changes or additional wear were noted in the journal bearings.

Sealing members were designed and installed in both turbine stators to eliminate the internal bypass. The turbine stator angles were not modified. A t-c set-driven oil pump was installed on the open-cycle test facility to provide a load on the t-c set gearbox. At the end of December, the t-c set was being reassembled in anticipation of further testing early in January.

<u>b. Stratos Turbine-Compressor Set (TCS-670A):</u> Following disassembly and inspection of the TCS-670A (Ref. a), Stratos was requested to submit a failure analysis report and a proposal for modification and repair of the unit. This proposal, and AGN's recommendations based on this report, were submitted to the Government (Ref. 17). The redesign proposed by Stratos incorporated four significant changes:

- Use of radial dowel pins between the nozzle and stator support rings and the nozzle support housing

- An increase in the diameter of the turbine interstage seal

- A change in turbine materials

- Redesign of the nozzle and stator blading

AGN's evaluation indicated that these modifications would significantly improve the structural integrity and performance of the TCS-670A, but that several critical areas in the design would require further analysis and corrective action if the unit were to be rebuilt. In view of the schedule and the cost involved, AGN recommended that it was not in the best interests of the Government to modify and rebuild the TCS-670A. At the direction of the USAEC, arrangements were being made at the end of December to provide for the secure storage of the TCS-660A until further notice.

A report of the closed cycle tests was published (Ref. 18).

<u>c. Alternator Development:</u> The ML-1 alternator was installed in the "caseless" test rig during early October. No-load, no-excitation rotational tests up to full speed revealed no vibration and demonstrated good bearing alignment. However, when the unit was excited, the roller bearing at the drive end housing immediately over-heated and failed. The failure was attributed to excessive flexibility in the caseless rig which allowed the fixture to warp under excitation loads and cause severe bearing misalignment. The alternator was then installed in the structural plastic case. Test of the unit in this configuration yielded results identical with those discussed above; the bearing in the drive end housing failed immediately after excitation.

Investigation of the failure first centered about the possibility of a bent rotor shaft. The unit was disassembled and the shaft measured. The measurement data indicate that the shaft is true although some uncertainty must be associated with this conclusion because of damage to the shaft resulting from the bearing failure. Further evaluation has led to the preliminary conclusion that the laminations in the drive end housing (provided to prevent the formation of eddy currents) prevent the flux in the homopolar air gap from equalizing. This highly unbalanced flux causes unbalanced electromagnetic forces which, in turn, are responsible for the bearing failure. The solution to this problem would require increasing the diameter of the rotor at the flux distribution ring and increasing the length of the unit.

Further development of the brushless alternator will be postponed because the future work will probably consist of the procurement of a commercial alternator to ML-1A specifications for incorporation and testing on the ML-1 power conversion skid.

The final design report for the brushless alternator was being prepared at the end of the report period.

d. Power Conversion Improvement:

1) Improved Precooler Design and Development: A re-evaluation of the improved precooler design indicated the desirability of providing a precooler to satisfy ML-1A as well as ML-1 requirements. The proposed ML-1 bowed-tube precooler was unacceptable for ML-1A use because of excessive package height (> 132 in.). A specification was prepared for a precooler which will satisfy ML-1A requirements and can be tested on the ML-1 skid to evaluate thermodynamic and mechanical performance. This precooler specification was submitted to prospective vendors and bids were requested. Responses to these bid requests will be received in February. These bids and AGN's recommended improved precooler program will then be submitted to the USAEC for review. The final design report for the bowed-tube precooler was being prepared at the end of December.

2) Improvement for Air Cycle Operation: The Cardair make-up compressor, a non-prototype, low-contamination type air compressor, was acceptance tested. These tests revealed that the 7-1/2 hp motor supplied with the unit is too small to drive the compressor at rated performance levels. The vendor was informed of these test results and agreed to substitute a 10 hp motor without additional cost. Testing will continue when a larger motor is installed.

The prototype hydrostatic face seal for the t-c set, similar in principle to a hydrostatic thrust bearing, was modified to increase the lift-off forces by increasing the leakage rate. The modified seal was tested; slight wear was observed at one position after approximately two hours of rotation at 25% speed. This indicates that the seal and the rotating face are misaligned. A modification was designed which provides even distribution of lift-off forces around the circumference of the seal; this will prevent the misalignment observed in the earlier test.

An interim report on the air cycle program,detailing the progress to date and presenting a recommended program,was published (Ref. 19).

3.3 Instruments and Controls

Fabrication of the spare source range and power range nuclear drawers was completed; these drawers were shipped to the ML-1 at NRTS with the nuclear drawer test jig. All spare drawers and the test jig, which will be used to store and test the drawers, were functionally checked in the ML-1 to assure interchangeability with existing equipment.

The design and fabrication of the improved process temperature chassis was completed. This chassis combines all process temperature circuitry into a single chassis with standardized circuits and "plug-in" components. Final testing of the chassis was in progress at the end of December; the chassis will be delivered and installed in ML-1 early in January 1964. Work on the improved ML-1 emergency power system was postponed when it became apparent that the system would not be applicable to ML-1A. The dc emergency system now operable at the ML-1 facility satisfies all ML-1 testing requirements.

Approval to proceed with final design and fabrication of the improved ML-1 speed and temperature controller is expected early in January.

3.4 Fuel Elements

a. Thermal and Neutronic Analysis: Work was performed to improve the calculational techniques used to analyze heterogeneous water-moderated reactors. A complex multigroup transport calculation was performed on a single cell of the GCRE-IA (plate-type element) critical subassembly, including both fast and thermal groups. The calculations for the one-dimensional plate-type element are much simpler than those for the two-dimensional pin-type elements but the shape and energy of the flux distribution outside of the fuel bundle are expected to be very similar for both types of elements. The data obtained from the calculations will be checked against experimental results. The results of this work will increase the understanding of heterogeneous water-moderated cells, and should provide greater insight into the neutronics of ML-1 type systems.

b. ML-1-II Burnable Poison Development: A trial quantity of europia-bearing stainless steel burnable poison foils was received. The 12 foils consist of a europia-titantia mixture dispersed in low-silicon stainless steel and clad with stainless steel. The foils are 0.010-in. thick and the cladding is from 0.0015- to 0.002-in. thick. The "active" portion of the foil is 22.12- by 5.147-in. and contains 4 gm of uniformly distributed europia. The certified analyses for the elemental powders used in the dispersion are shown in Table 3-1 and the certified analysis of the europia-titantia mixture is shown in Table 3-2.

TABLE 3-1 - CERTIFIED ANALYSES OF POWDERS USED IN BURNABLE POISON DISPERSION

Element	Nickel Powder wt%	Chromium Powder wt%	Iron Powder wt%
Carbon	0.08	0.02	0.02
Silicon	0.001	0.001	0.001
Cobalt	0.002	0.001	0.001
Nickel	99.917	--	--
Chromium	--	99.978	--
Iron	--	--	99.978

TABLE 3-2 - CERTIFIED ANALYSIS OF EUROPIA-TITANTIA BURNABLE POISON MIXTURE

Compound	wt%
Eu_2O_3	68.75
TiO_2	31.15
SiO_2	0.015
Rare Earth Oxides and Impurities	0.085

During the development of the europia-titantia mixture, the vendor performed a stability test in which pellets of representative composition were immersed in boiling water for 48 hours. No loss of weight was observed as a result of this exposure and the structural integrity of the pellets was not altered.

Examination of the foils as delivered by the vendor showed that the dimensional specifications had been met and that the cladding is continuous and free of blisters and porosity. Typical foils are shown in Fig 3-2; the light and dark areas are caused by uneven reflection of light and are not apparent by visual inspection of the foils. Compatibility tests in demineralized water at 190°F and in reference gas at 800°F were initiated during the report period. Metallographic evaluation of the foils was in progress at the end of December to determine the integrity of the cladding, the bond between the cladding and the matrix, the density of the matrix, and the dispersion and particle size of the europia and titantia.

The development of a non-destructive method for determining the uniformity of europium distribution continued throughout the report period. The use of an isotopic neutron source with subsequent determination of the capture gamma activity was demonstrated to be impracticable because of the high gamma activity of the source. A technique using a neutron generator and subsequent determination of the capture gamma rate was being evaluated at the end of December.

c. IB-14R Metallurgical Evaluation: Metallographic examination of sections of the IB-14R pin 17 (which contained 98.1% nitrogen after irradiation) was performed. The purpose of the examination was to determine if the embrittling pi-phase observed in the cladding of some IB-8T-1 pins, and subsequently attributed to nitrogen in the pin, could be detected. The structure of the cladding is similar to that of other IB-14R pins (Fig 3-3). No embrittlement or second phase formation (other than carbide precipitation) was observed. It was concluded that the absence of pi-phase was attributable to the presence of air in the pin (instead of reference gas as in the IB-8T-1) or to the relatively short exposure.

Sections of the IB-14R inner liner were also evaluated for possible embrittlement. The samples were ductile and possessed a structure free of embrittling phases. Oxidation attack was minimal.

d. IB-17R In-Pile Test: Post-irradiation examinations of the IB-17R-1 test element were completed during the report period. Preparation of the final report of the examination was initiated.

The AGN-GETR air-cooled loop continued to operate with test elements IB-17R-2 and -3. Typical operating conditions are shown in Table 3-3.

The IB-17R-2 element is scheduled for removal on 6 January 1964, after 5819 hr of nuclear operation. The IB-17R-3 (uninstrumented) test element is scheduled to remain in the loop until October 1964.

A number of repairs and modifications were completed in the AGN-GETR loop during the quarter. Compressors were rebuilt and re-installed in the No. 1 and 2 positions. Temperature probes were repaired and installed in both

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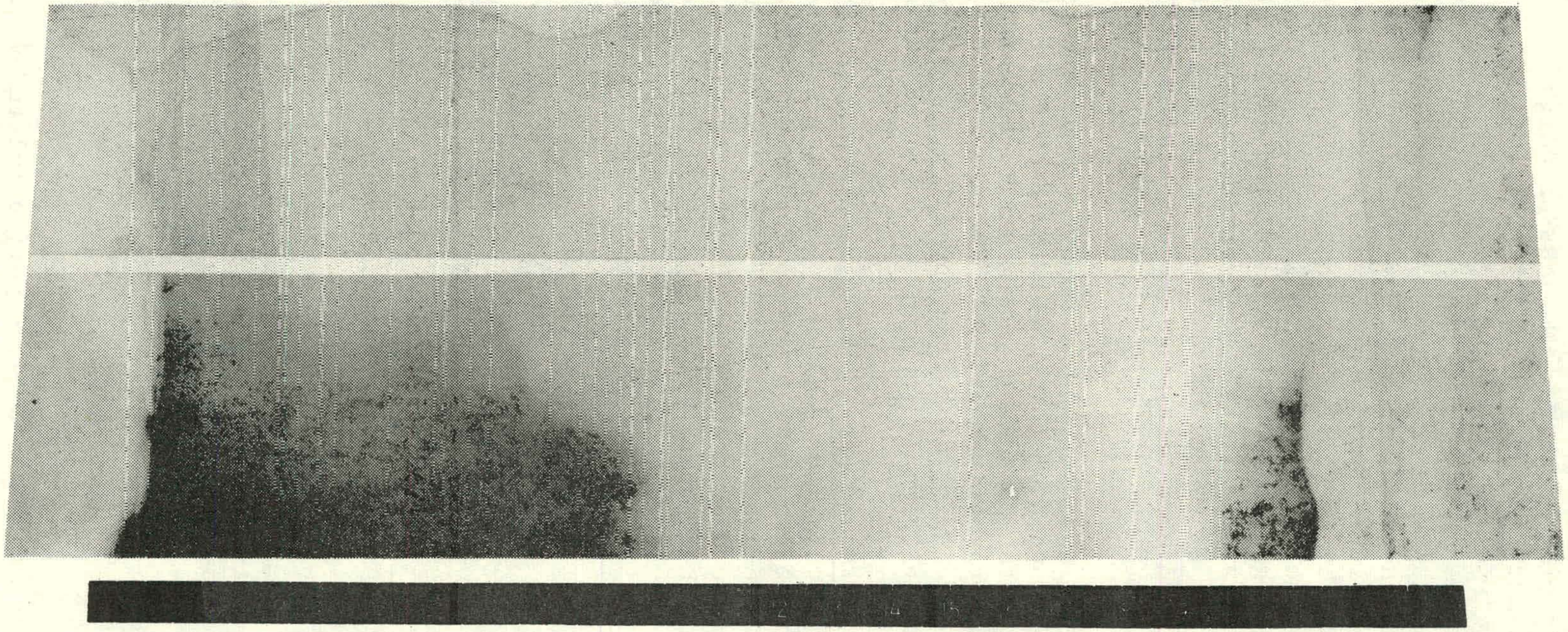


FIGURE 3-2. EUROPIA - TITANIA BURNABLE POISON FOILS

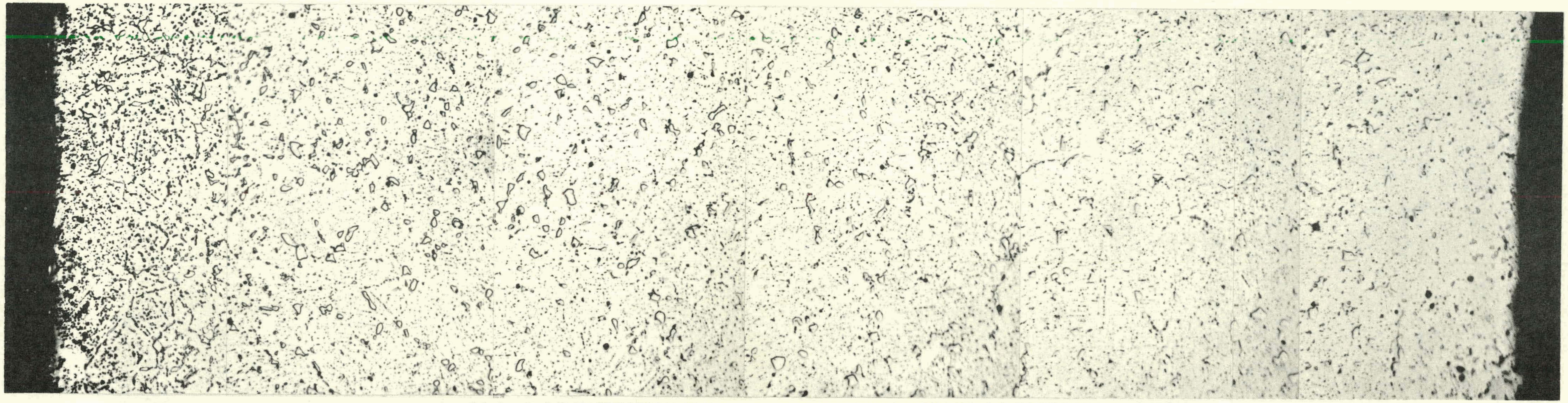


FIGURE 3-3. IB-14R HASTELLOY X CLADDING AFTER IRRADIATION PIN 17, 4.0 INCHES FROM BOTTOM

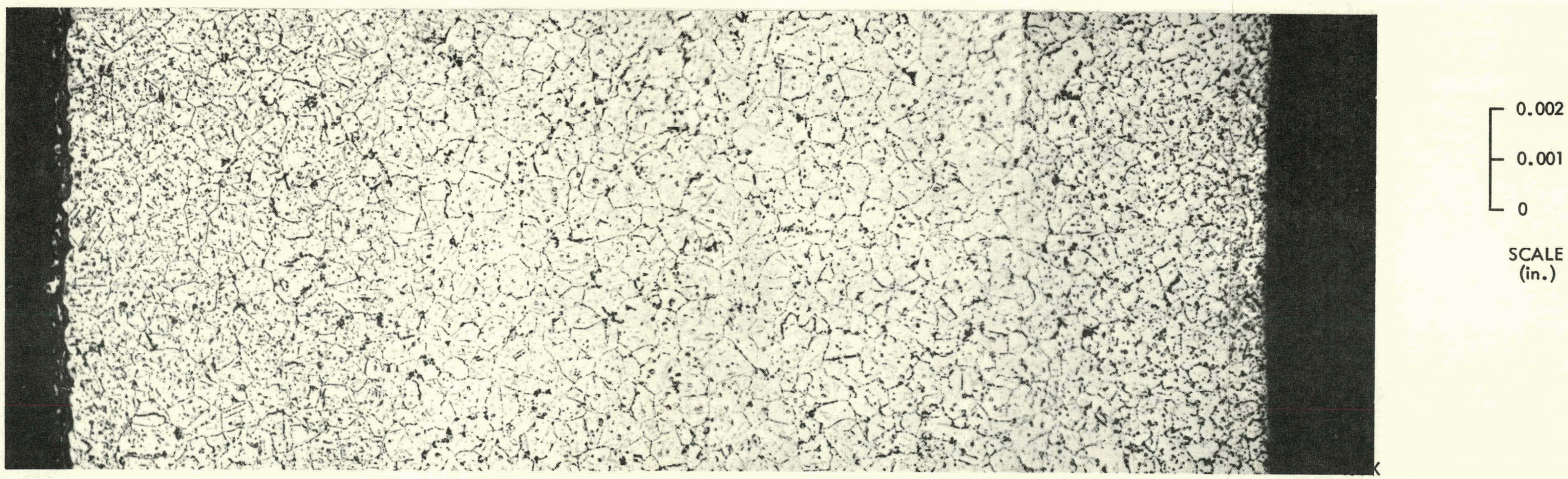


FIGURE 3-4. IB-17R HASTELLOY X CLADDING PIN 18, 6.3 INCHES FROM BOTTOM

TABLE 3-3 - TYPICAL GETR LOOP OPERATING CONDITIONS, DECEMBER 26, 1962, 2300 HOURS

GETR power, Mw	30.3
Rod bank position, in.	25.6
Pool temperature, °F	100-106

	Facility Tube 2	Facility Tube 3
Element designation	IB-17R-3	IB-17R-2
Inlet temperature, °F	800	795
Outlet temperature, °F	1200	1195
Cladding temperature, °F		
Thermocouple 6,	-	1240
13	-	1095
16	-	1160
Pressure drop, psi	10.9	12.0
Flow, lb/hr	1900	2050
Test element power, kw	61.2	64.0
Accumulated exposure, hr	4354.1	5533.4

facility tubes. Micro-metallic filters were installed on both sides of the test section in both facility tubes.

Analysis of the particulate matter in the GETR loop was initiated during the period. One sample was analyzed from each of the following locations:

Sample 1: Facility Tube 2 Fiberfrax filter

Sample 2: Facility Tube 3 Fiberfrax filter

Sample 3: Facility Tube 2 downstream micro-metallic filter

Sample 4: Facility Tube 3 downstream micro-metallic filter

Sample 5: Inside of compressor

The Fiberfrax filters had been in use since December 1960. The micro-metallic filters were used only in GETR Cycle 48, which ended on 27 October 1963.

The particulate samples were dissolved in dilute hydrochloric acid and the cobalt and iron were chemically separated. The separated samples were analyzed by radiochemical techniques and a gross gamma scan of the residue from the separation was used to determine manganese, chromium and antimony concentrations (Table 3-4).

The count rates in Table 3-4 are stated in terms of the dpm/mg of iron in the sample. If the assumption is made that the percentage of iron was the same in all samples, the count rates quoted are proportional to the activity of the total sample. It is apparent from the table that Cr-51 is the most active isotope detected. It is also apparent that the micro-metallic filters collected material recently activated in the test section.

TABLE 3-4 - RELATIVE GAMMA COUNT RATES FOR AGN-GETR LOOP SAMPLES

	Isotope Count Rate, dpm/mg Fe x 10^3*					
Sample	Co-60	Co-58	Fe-59	Cr-51	Sb-124	Mn-54
1	40	72.7	57.2	45.7	16.1	102
2	290	191	17.3	6820	44.7	283
3	680	1080	90.6	10800	51.0	---
4	210	290	181	34500	136	576
5	3.9	4.03	0.97	33.5	2.11	5.97

*Corrected for decay to time of sampling

The compressor sample was also analyzed by spectrochemical and X-ray diffraction techniques. When ignited, the sample smoldered (in air) and a weight loss of 33% was observed. This weight loss was probably associated with hydrocarbons and free carbon in the sample. After ignition, the sample was determined to be 99+% Fe_2O_3. Spectrographic analysis revealed several trace elements (Table 3-5).

TABLE 3-5 - SPECTROGRAPHIC ANALYSIS OF GETR LOOP COMPRESSOR SAMPLE

Element	ppm of Total Sample	Element	ppm of Total Sample
Ag	1	Mg	1
Al	10	Mn	50
Sb	< 10	Mo	< 10
B	< 1	Na	< 100
Ba	5	Ni	100
Be	< 1	Nb	< 100
Bi	< 1	Si	500
Ca	10	Sn	100
Cb	50	Ta	< 100
Cd	< 1	Ti	< 1
Co	< 10	V	< 10
Cu	50	W	< 100
Fe	Major	Zn	< 100
Pb	< 1	Zr	< 100

Wet chemical analysis of Sample 5 indicated that the sample contained 1.6 wt% SiO_2. X-ray diffraction analysis after ignition indicated that α-Fe_2O_3 was the major constituent; trace quantities of $CaO \cdot SiO_2 \cdot n\,H_2O$ and $3\,CaO \cdot SiO_2 \cdot n\,H_2O$ were also detected. A silicate of the type detected had been used in the loop as a molecular sieve drying agent.

e. IB-17R-1 Metallographic Evaluation: Metallographic samples from the IB-17R-1 in-pile test element were evaluated at the M-E-ATR hot cell facilities at NRTS. Sections were obtained from pins 6, 12 and 18 at the upper and lower transitions from finned to unfinned tubing regions, at the point of maximum cladding temperature, and at the top of the fuel stack.

Although the exposure time of the test element was too short to provide fuel element lifetime data, the following observations were made:

1) The microstructure of the Hastelloy X cladding closely resembled that of duplicate material exposed for the same duration out-of-pile at the same temperatures. The microstructure of the IB-17R-1 cladding of pin 18 at 6.3 in. from the bottom (X/L = 0.75) is shown in Fig 3-4.

2) Relatively little oxidation of the cladding was observed. The maximum thickness of oxide scale was 0.0005 in. and the maximum depth of intergranular oxidation was 0.0006 in.

3) The fuel pellets were generally not cracked. Some of the large UO_2 particles were cracked but the cracks did not extend into the BeO matrix. No phase rearrangements were observed.

4) A thin layer of a foreign material was observed on the inside diameter of the cladding of pin 12. This material is unlike any material observed to date during fuel element examinations. Further investigations will be made.

5) The cladding had not collapsed onto the fuel pellets as indicated by large clearances between the fuel pellets and the cladding.

f. IB-17R Out-of-Pile Support: Coolant/cladding and cladding/fuel compatibility tests are being performed out-of-pile to provide control data for comparison with the IB-17R in-pile tests.

Specimens of IB-17R-1 tubing exposed to air at various temperatures for 1179 hr out-of-pile were tensile-tested and evaluated metallographically. The results of the tensile tests are shown in Table 3-6.

The microstructures of the samples exposed at various temperatures are shown in Fig 3-5. It can be seen that a precipitation has occurred in the specimens. The precipitate is very fine after the lower temperature exposures but coarser after higher temperature exposures. (This situation is normal for Hastelloy X specimens aged in air.)

The test of the IB-17R and ML-1 fuel/cladding compatibility capsules was interrupted after 136 hr of exposure in air. One capsule from the IB-17R set was cut open and examined metallographically. Examination revealed that about half of the cladding had collapsed onto the fuel. The test was resumed to expose the remaining capsules to 300 psi air at 1800°F to accumulate a total of 500 hr. After this exposure, visual examination revealed that the cladding had collapsed uniformly onto all the fuel pellets. The capsules were then placed in an atmospheric furnace to accumulate the balance of the 10,000-hr, 1800°F data.

TABLE 3-6 - TENSILE PROPERTIES OF IB-17R TUBING HEATED IN AIR FOR 1179 HOURS

BMI Sample No.	Exposure Temp., °F	Ultimate Tensile Strength, psi	Yield Strength, psi	Elongation %
FKA-81	Unexposed	117,000	*	45.0
FKA-115	Unexposed	120,100	*	47.0
FKA-80	1300	124,800	65,300	15.7
FKA-80	1450	125,700	60,200	19.7
FKA-80	1750	105,000	52,300	37.7
FKA-80	1750	106,300	50,000	34.9
FKA-83	1300	107,300	50,800	33.8
FKA-83	1450	114,100	*	38.5
FKA-83	1750	104,300	47,800	36.3
FKA-83	1750	113,500	50,200	30.0

*Data invalid.

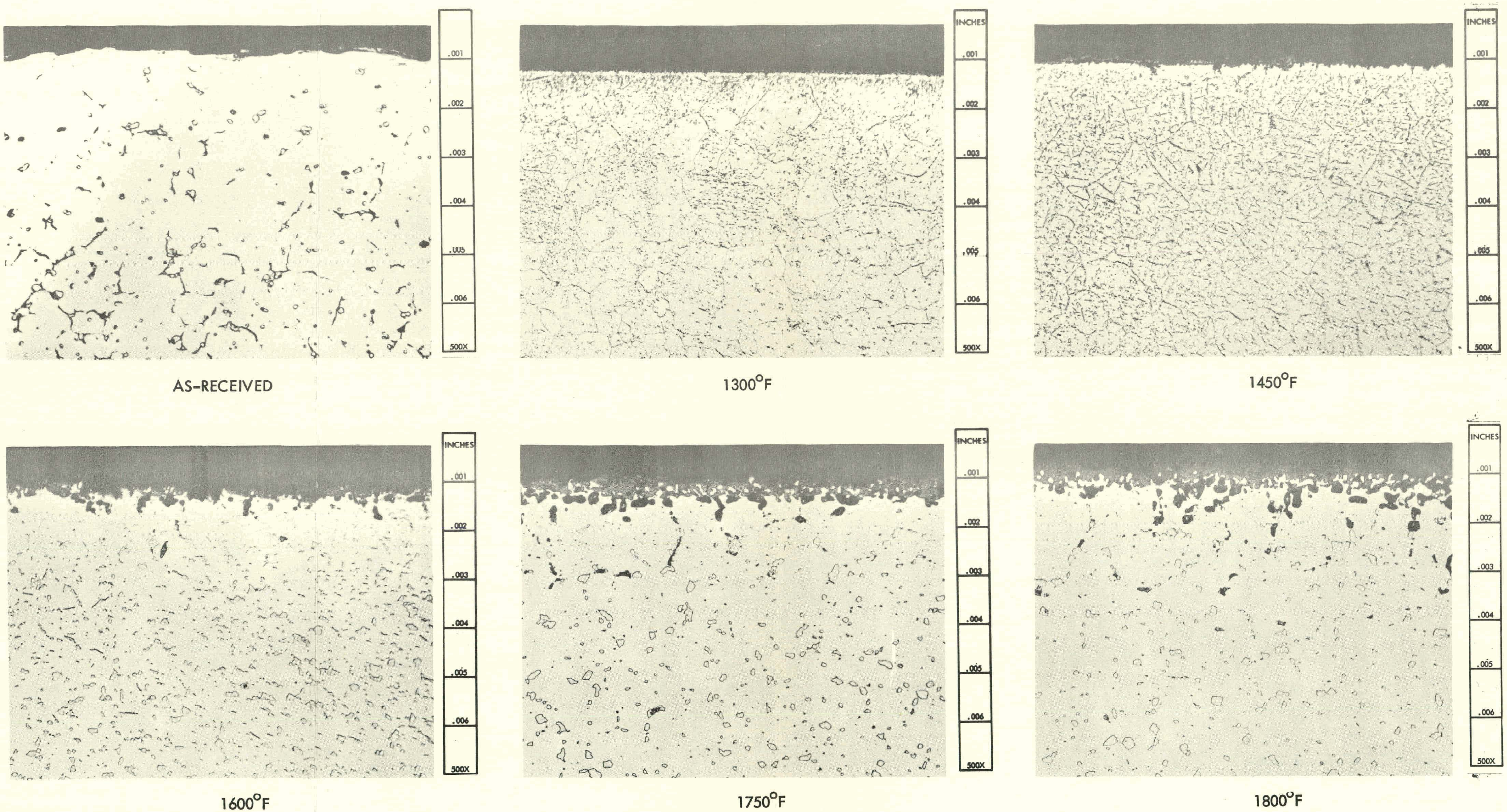


FIGURE 3-5. IB-17R HASTELLOY X TUBING EXPOSED TO AIR FOR 1179 HOURS OUT-OF-PILE

11.2-64-301

III. ML-1A PROJECT

4.0 ML-1A PRELIMINARY DESIGN

The ML-1A preliminary design scope, cost estimate, and schedule were finalized in discussions held with USAEC personnel at AEC Headquarters, Germantown, Md, in October 1963. The significant agreements of the meeting were documented (Ref. 20). A detailed cost estimate and statement describing the scope of work to be completed during the ML-1A preliminary design effort was published (Ref. 21). A detailed schedule was developed, showing the major milestone dates as well as schedules for individual items (Ref. 22).

The elapsed time required for the preliminary design effort is estimated to be seven months; the estimated completion date is 15 May 1964.

Significant milestones in the program are as follows:

- Submittal of the preliminary ML-1A systems analysis and energy balance — due 6 January 1964 (this submittal was made by Ref. 23)

- Determination of the basic reactor shielding design concept: This will involve an evaluation of the tradeoffs required to meet the Qualitative Materiel Requirements (QMR) shielding requirements and the 15-ton weight limit. These requirements will be considered along with any possible increase in operational or shutdown dose rate in areas not defined by the QMR — due 6 January 1964.

- A design decision regarding the deoxygenation of moderator water: The plant weight and startup procedure implications and ancillary equipment requirements of a deoxygenation system will be considered in arriving at the decision — due 6 January 1964.

- Determination of plant startup power requirements: The auxiliary loads required during startup will be determined, and the feasibility of starting up the plant with the auxiliary power source specified in the QMR (60 kw) determined — due 2 March 1964.

- Definition of alternator capacity: The net output power of the plant as a function of ambient temperature will be determined, the weight and size of alternators of various capacities will be established and the implications on the power conversion skid weight and envelope will be determined. The capacity of the alternator will be defined in consideration of these factors — due 2 March 1964.

- Approval of control cab, console, and rack layout: The preliminary cab layout, including recommendations from the Human Engineering Laboratory (HEL) and the study to define the instrumentation requirements will be completed and the layout design submitted for approval — due 2 March 1964.

The ML-1A performance specification was reviewed, and a proposed amendment, to make the specification consistent with the ML-1A design, was forwarded to the USAEC for review (Ref. 24).

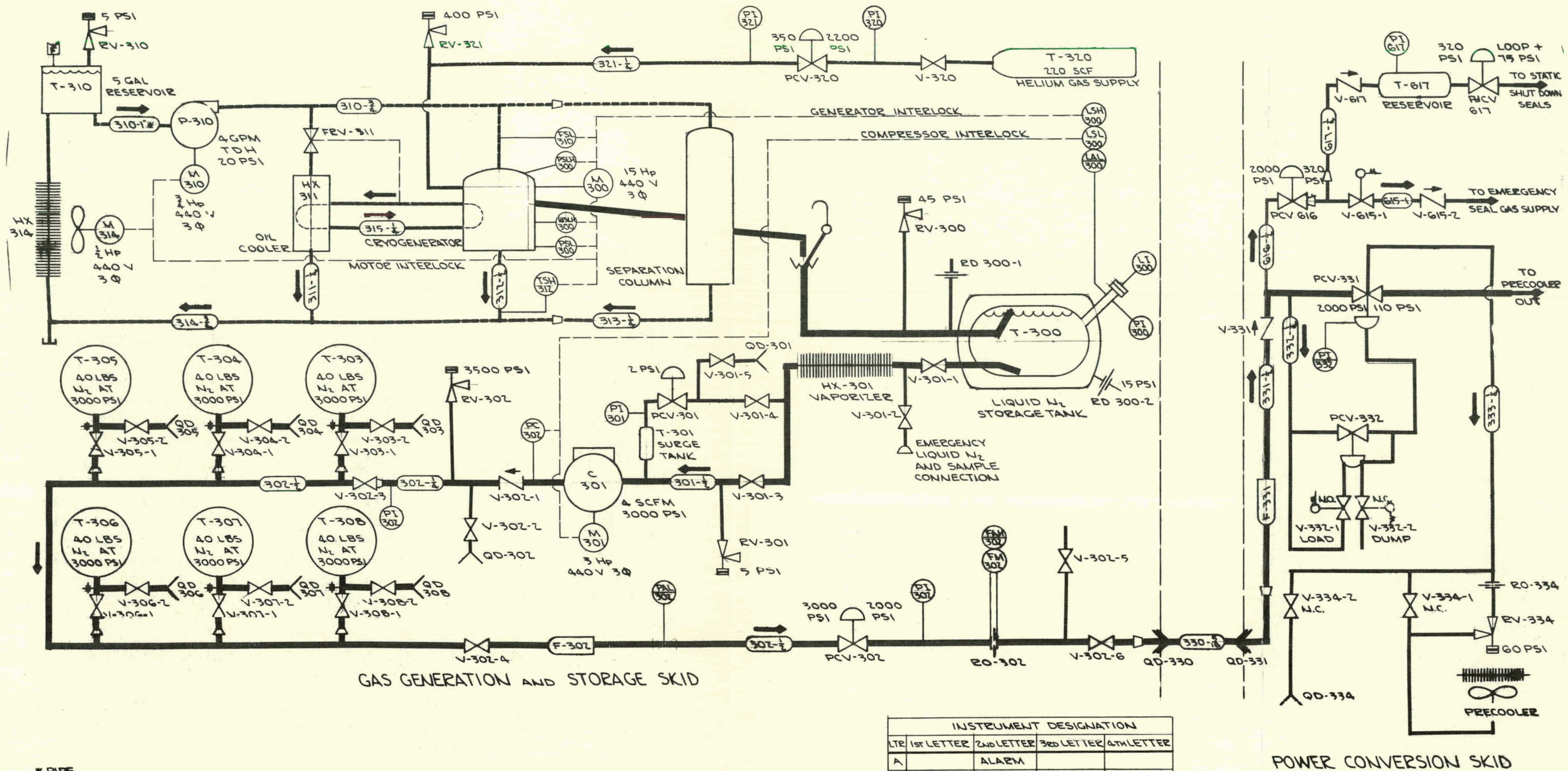
4.1 Reactor and Auxiliaries

a. Gas Handling Skid: Two gas make-up and handling skid concepts were investigated; the ML-1 gas storage concept and nitrogen generation in the field, a new concept. It was evident that the gas storage concept is not acceptable if realistic make-up rates (of the order of 0.5 scfm) are stipulated for the ML-1A plant. At a leak rate of 0.5 scfm, 96 standard nitrogen cylinders a month are required to satisfy the make-up requirements; additional gas is required for loop charging and for reserve.

Investigation revealed that nitrogen generation is feasible for field operation of an ML-1A plant. In this concept, air is liquified in a Stirling cycle expansion engine and separated into liquid nitrogen and gaseous oxygen in a distillation column. The oxygen content of the product is controlled by an oxygen blow-off regulator valve on the distillation column. The liquid nitrogen is stored in a 55 gallon, insulated vessel. The liquid nitrogen is drawn from the vessel through a vaporizer by a 3-stage 3000 psig compressor and the gas is then stored in high-pressure storage spheres. The generating and storage equipment is located in the vicinity of the control cab and is manually operated. The gas is transferred to the primary loop by a high pressure hose and remote-controlled regulators mounted on the power conversion skid. The make-up gas supply also supplies emergency seal gas and static seal gas.

The power requirements of the liquification equipment are approximately 13.2 kw and those of the compressor are approximately 2.4 kw. The gas generating capability is approximately 3 scfm, consequently the equipment can be operated intermittently and, normally, only during periods of low power plant demand. The liquification equipment and the compressor are commercially available, of proven reliability, and have had extensive military application. The equipment is considerably more compact than the ML-1 gas storage skid and, as a consequence, makes approximately 150 cu. ft. of storage space for ancillary equipment available on the skid.

A process and instrumentation diagram for the gas handling skid and a component layout were completed (Fig 4-1). A liquid nitrogen storage vessel and the high pressure storage spheres were specified.



INSTRUMENT DESIGNATION				
LTR	1st LETTER	2ND LETTER	3RD LETTER	4TH LETTER
A		ALARM		
C		CONTROLLER		
F	FLOW			
H			HIGH	HIGH
I		INDICATOR		
L	LEVEL		LOW	LOW
M		MONITOR		
P	PRESSURE			
Pd	DIFFERENTIAL PRESSURE			
S		SWITCH		
T	TEMPERATURE			

* PIPE

LEGEND

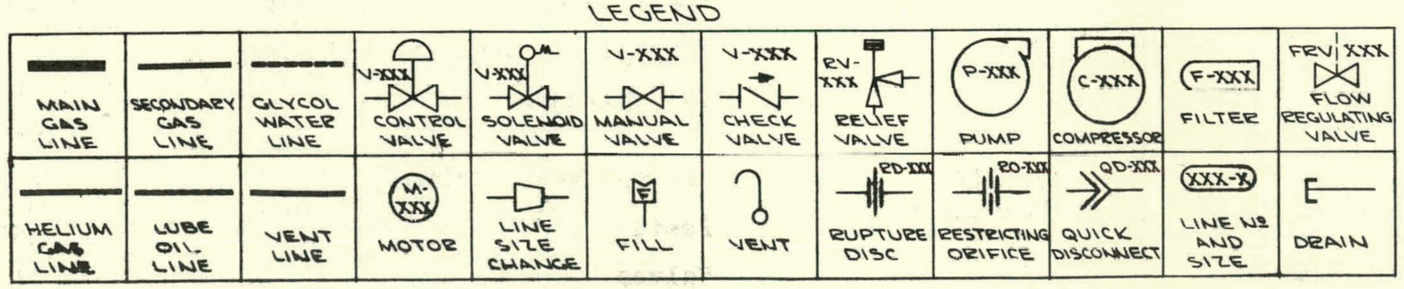


FIGURE 4-1. P&I DIAGRAM: ML-1A GAS GENERATION AND STORAGE SKID

1.2-64-302

Argon activation in the primary gas loop was investigated. The majority of the liquified argon is removed with the oxygen in the distillation column. The Ar-41 concentration in the main loop was calculated to be 0.02 curies at saturated conditions; this concentration will not cause any activation problems.

b. Cable and Cable Handling Equipment: A summary of the cable wire requirements for the ML-1A power plant was completed, based on the results of the instrumentation study discussed in Section 4.2a. The wires were being grouped into cables at the end of December. Preliminary results of this work indicate that approximately 15 cables will be required between the control cab and the power plant. The total cable weight is estimated to be approximately 6000 lb. Inquiries were sent to vendors of cable reels to determine the design and performance characteristics of commercially available reels.

c. Deoxygenation Equipment: The conceptual design of a deoxygenation system suitable for the ML-1A plant was completed. The proposed system involves pressurization of the moderator system to a minimum of 23 psia (partial pressure) with hydrogen gas. This pressure insures that a sufficiently high concentration of dissolved hydrogen (25 cc of hydrogen at STP per liter of water) is present to suppress radiolytic decomposition of the moderator water and, hence, oxygen production. The initial filling of the moderator system is accomplished by passing demineralized water through a small (2.5 cu.ft) deoxygenation column. Bypass flow through the deoxygenation column is not required during operation. The total operating pressure of the moderator storage tank is estimated to be approximately 38 psig. A rupture disc sized to vent a hydrogen-air explosion is located on the tank and is designed to rupture at 135 psig. Primary pressure relief for gas overpressure or steam formation is provided by a relief valve set at 50 psig. This pressure setting is a factor of 2.4 less than the pressure required to collapse the reactor pressure tubes at one atmosphere internal pressure. Hazards associated with hydrogen-air explosion or fire are minimized by proper design and operating procedures. Installation of the deoxygenation system is estimated to increase plant weight by 710 lb (Table 4-1).

TABLE 4-1 - INCREASE IN PLANT WEIGHT ASSOCIATED WITH DEOXYGENATION EQUIPMENT

Location	Item	Net Weight Increase, lb
Reactor Package	Moderator Storage Tank and Fixtures	290
	Lower Moderator Seal Bellows	20
	Control and Regulating Valves	45
	Gas Cylinders	75
		430
Ancillary Equipment	Deoxygenation Column	150
	Resin	110
	Valves	20
		280
	TOTAL INCREASE IN PLANT WEIGHT	710

It was concluded that the deoxygenation system described above was feasible but should not be incorporated in the ML-1A power plant design. This conclusion was based on consideration of the added weight and complexity; the lack of opportunity to demonstrate the design; the increased hazards (and possible difficulty in obtaining safety analysis approval); and finally, the absence of a firm requirement for deoxygenation of the moderator water.

A report describing the deoxygenation system design considerations will be published in the next report period.

d. Pressure Vessel: The design of mechanical fixtures for testing the ML-1A tube-to-tube sheet joint was completed and fabrication was initiated. Provision is made for two types of mechanical tests:

- A split mock-up of a tube sheet hole will be used to permit removal of the tube after rolling for examination by sectioning

- A solid tube sheet hole mock-up will be used for mechanical push and pull tests to determine the strength of the tube-to-tube sheet joint

The calculation of stresses in the ML-1A tube bundle was initiated. The principal differences between the ML-1 and the ML-1A tube bundles are the material (AISI Type 347 stainless steel; ML-1 is AISI Type 304 and 321) and the tube wall thickness (0.030 in.; ML-1 is 0.020 in.). The TSA-1 code, developed for calculating ML-1 tube bundle stresses, is being used for the ML-1A analysis. The results of the calculations will be presented in parametric form as curves to display the relative significance of the variables involved (tube sheet thickness, pressure tube wall thickness, etc.) for optimizing the tube bundle design. Requests were made of Mare Island and the Bureau of Ships for the Navy's NR-S-1 Pressure Vessel Code to be used as a guide for the ML-1A analysis. This code had not been received at the end of the report period.

e. Shielding: Earlier ML-1 testing at NRTS has demonstrated that the operational dose rate at the control cab is 8.2 mrem/hr with the expedient shielding in place and the shutdown dose rate 24 hours after shutdown at the truck cab would be 19.7 mrem/hr after extended operation. The dose rate requirements for ML-1A are 5 mrem/hr at the control cab during operation without expedient shielding, and 15 mrem/hr in the shutdown case.

The design criteria for the ML-1A reactor skid were established during the report period. These criteria include the definition of the package size and weight; the range of environmental conditions for startup, operation and shutdown; the radiation limits during operation and after shutdown; the maintenance requirements; the operating supplies requirements; and the setup and deployment time limits. These criteria are based on applicable paragraphs of the QMR and of the ML-1A performance specifications.

The mechanical design of those changes required to meet the shielding and package weight criteria were completed. The shield tank diameter was increased and the shield water height was increased by 2 ft (with a collapsible tank extension) to meet the 5 mrem/hr operational dose rate requirement. The shield water solution was changed from 10 wt% boric acid to 2 wt% boric acid to satisfy the 18-hr startup time requirement, with 60 kw maximum of auxiliary power. The shield modifications compensated for the adverse effect of this reduction in boric acid concentration on the operational and shutdown radiation levels.

The shutdown dose rate requirement was satisfied by specifying extremely low limits on the allowable amounts of cobalt and tantalum in the stainless steel (AISI Type 347) used in the reactor and by increasing the polystyrene shield thickness on the cab side of the reactor from 2 to 6 in.

The reactor inlet gas duct was redesigned to accommodate the relocation of components on the power conversion skid required by the use of a commerical alternator. This redesign did not significantly affect the shielding characteristics of the reactor shield. Significant design changes were made to the reactor skid shield tank structure, the reactor support structure, the inlet gas duct, the reactor end shielding, and the reactor skid tie-down structure to maintain the weight of the skid within the 15-ton limit. The redesign of these components resulted in a weight reduction of 1962 lb. The ML-1A reactor skid nominal design weight is 29,998 lb.

Shielding analyses were performed to determine the effect of the mechanical design changes on the reactor shield performance. It was concluded that all ML-1A shielding requirements, both operational and shutdown, could be satisfied. However, the magnitude of the performance improvement which the shielding redesign effort is attempting to achieve is so small that it is difficult to predict with accuracy the effect of the design changes. Verification of the analysis will require full power tests at the ML-1 power plant. It is concluded, nevertheless, that the modifications have a high degree of probability of resulting in a satisfactory shield design.

Investigation of the fabricability and availability of the low-cobalt, low-tantalum AISI Type 347 stainless steel determined that there are no unusual fabrication problems. The material is not a standard product but several steel producers indicated that the ML-1A requirements can be satisfied on special order. Type 3003 aluminum alloy will be used in place of the Type 6061 used on ML-1 because of the superior corrosion resistance of the 3003 alloy in the boric acid shield solution.

The following work was in progress at the end of December:

- Additional shielding analyses were being performed to more fully define the shield effectiveness and to assist in the design of a full power shielding test to be performed at ML-1.

- Stress calculations, final weight analyses, and detailing of mechanical design were being performed for incorporation in the final layout design drawings.

4.2 Instrumentation and Control Cab

a. Instrumentation Study: A detailed review of all ML-1 power plant instrumentation was made to determine the minimum instrumentation requirements for the ML-1A plant. An ML-1A instrumentation plan was developed; this plan includes the diagnostic instrumentation required during initial power plant testing and performance evaluation. This study made maximum use of experience in NRTS operations, the recommendations of the Human Engineering Laboratory, and the judgment and experience of the ML-1 Instrumentation and Control engineering personnel. The report of this study will be published in January 1964.

b. HEL Representative Visits: A representative of the Human Engineering Laboratory made two visits to AGN during the report period to participate in detailed discussions on the design of the ML-1A instrumentation and control cab. During the first visit, decisions were reached on the following: the annunciator system logic; maintenance, pre-startup adjustment and calibration facilities for nuclear instrumentation; circuit breaker design; elimination of the speed and temperature error meters; nuclear startup recorders; preliminary control cab, console and rack layout; cab illumination; cab heating and cooling requirements; a blackout curtain and illumination of indicators; meter relays; and meter design. During the second visit, the ML-1A instrumentation study was discussed and HEL recommendations incorporated; parallel operation of the ML-1A plant with other electrical power sources was discussed in detail; and operating procedures to accomplish paralleling were developed.

c. Control Cab Layout: A preliminary layout of the control cab was completed (Figs 4-2 and 4-3). A "U"-shaped control console is provided with a functional instrument display. Only four racks are required because of the use of instrumentation developed in the ML-1 Improvements Program, the use of a high performance air conditioner designed under Government contract, and other design changes discussed below. Consequently, space is available for a storage closet, gun rack, two chairs, and an escape hatch.

An initial weight summary of the ML-1A control cab was completed. Table 4-2 shows the ML-1A and the ML-1 weights. The weight decrease was achieved principally by use of instrumentation being developed under the ML-1 Improvements Program, the dc control concept, a modified emergency power system, and the use of the high performance air conditioner.

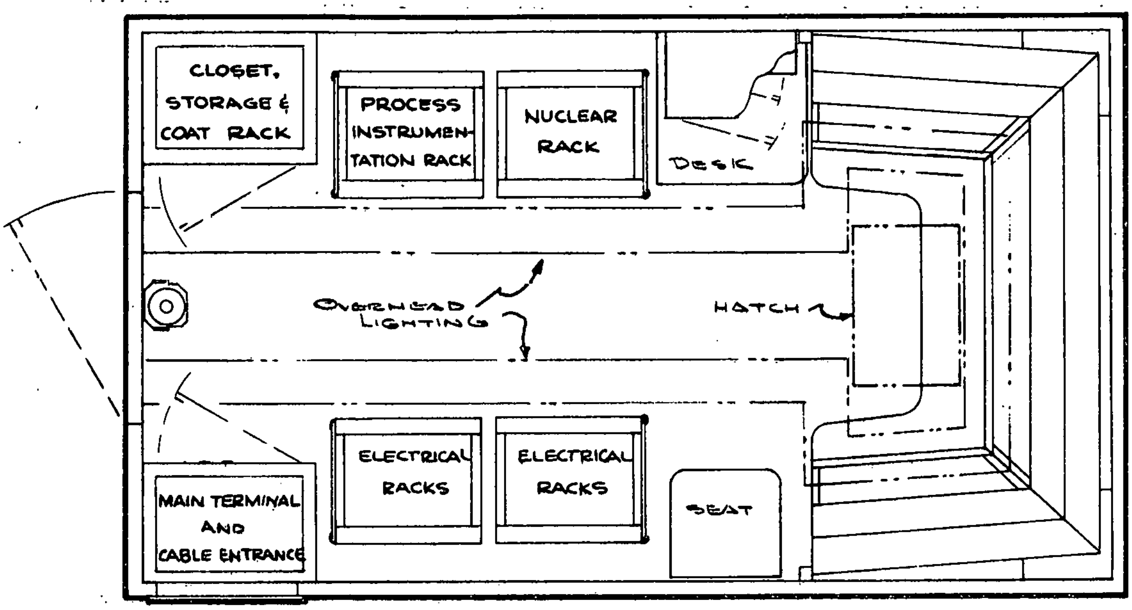
d. 28 Volt DC Control: A study was made to determine the optimum voltage for the ML-1A control system. Based on the results of this work, the decision was made to use a 28 v-dc system. The major advantage of the 28 v-dc system is in the simplification of the cab power distribution system by reducing the inverter power requirements, eliminating a separate ac voltage regulator, and combining a regulated power supply with the battery charger. The use of this system results in a weight decrease of approximately 400 lb.

e. Frequency Response: An analog computer study was completed to determine whether the ML-1 speed controller design was adequate to meet the essential and desirable transient frequency regulation requirements specified in the QMR. It was determined that:

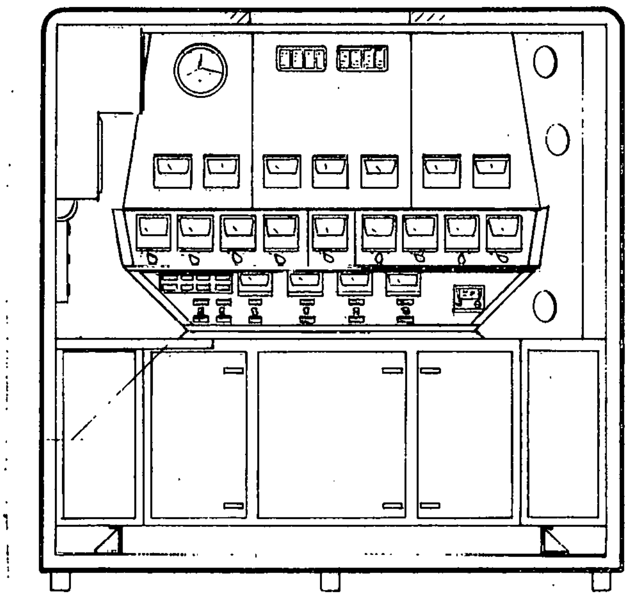
- The "required" speed recovery to ± 1/3% within 4 seconds following 50% load steps (per MIL-G-10328A) is satisfied for load steps in either direction. As shown in Fig 4-4, recovery to ± 1/3% is accomplished in one second for load drops and 3.2 seconds for load increases.

TABLE 4-2 - ML-1A CONTROL CAB
INITIAL WEIGHT SUMMARY

	PRESENT ML-1 Pounds	PROPOSED ML-1A Pounds
CONSOLE		
FRAME	300	300
PANELS AND INSTRUMENTS	478	300
RACK 1		
BATTERY CHARGER	200	175
BATTERIES	475	175
RACK 2		
CIRCUIT BREAKER PANEL	100	100
TRANSFER RELAY	100	15
3 KVA TRANSFORMER	50	--
INVERTER	180	50
3 KVA REGULATOR	175	--
RACK 3		
LOCKOUT RELAY PANEL	10	3
STATOR CURRENT AND GENERATOR PROTECTION CIRCUITS	50	10
ROD CONTROL CHASSIS	75	20
7-1/2 KVA TRANSFORMER	200	50
RACK 4		
WIND SPEED AND DIRECTION	50	--
ANNUNCIATOR POWER SUPPLY	50	50
SOURCE RANGE NUCLEAR	41	
INTERMEDIATE RANGE NUCLEAR	39	200
POWER RANGE NUCLEAR	47	
SCRAM LOGIC AND CLUTCH POWER	50	
RACK 5		
COMPRESSOR OUTLET HI-TEMPERATURE SCRAM	30	25
EVALUATE PRESSURE AND FAST PRESSURE LOSS	50	26
COMPRESSOR PRESSURE	30	--
LUBE OIL SUMP PRESSURE	30	--
SPEED/TEMPERATURE CONTROLLER	75	30
PROCESS TEMPS #1	40	--
CONDUCTIVITY INSTRUMENT	20	20
Rx OUTLET HI-TEMPERATURE SCRAM	35	14
PROCESS TEMPS #2	15	--
BEARING TEMPERATURE AND LIQUID LEVEL	50	27
TRANSFER COMP. PRESSURES	25	--
RACK 6		
SAM SYSTEM	40	25
AMBIENT TEMPERATURE	7	--
STORAGE PRESSURE	35	--
VIBRATION	30	20
OVERSPEED SCRAM	40	20
MISCELLANEOUS		
GENERATOR PROTECT RELAY	75	25
AIR CONDITIONING/HEATER	650	200
CHAIR	48	48
CAB ENCLOSURE	1200	1200
RACKS AND SHOCK MOUNTS	500	500
MAIN TERMINAL BOX	150	100
FIRE EXTINGUISHER	25	25
WIRING AND PLUGS	500	250
MISCELLANEOUS	--	500
TOTAL	6370 Pounds	4503 Pounds



PLAN



ELEVATION

FIGURE 4-2. ML-1A CONTROL CAB PLAN AND CONSOLE ELEVATION

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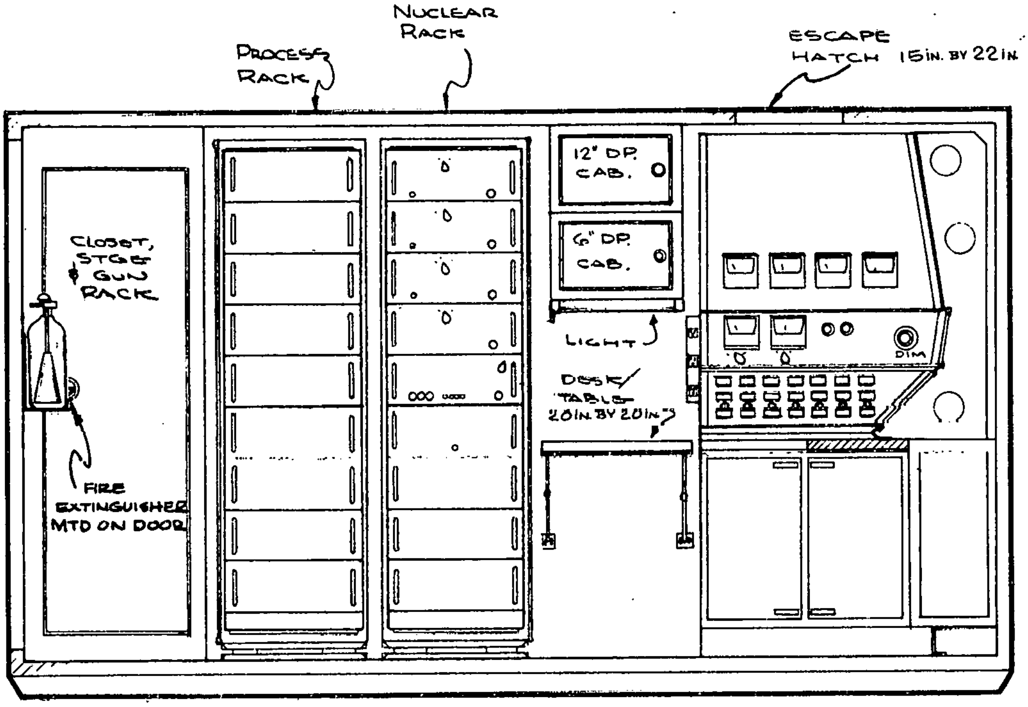
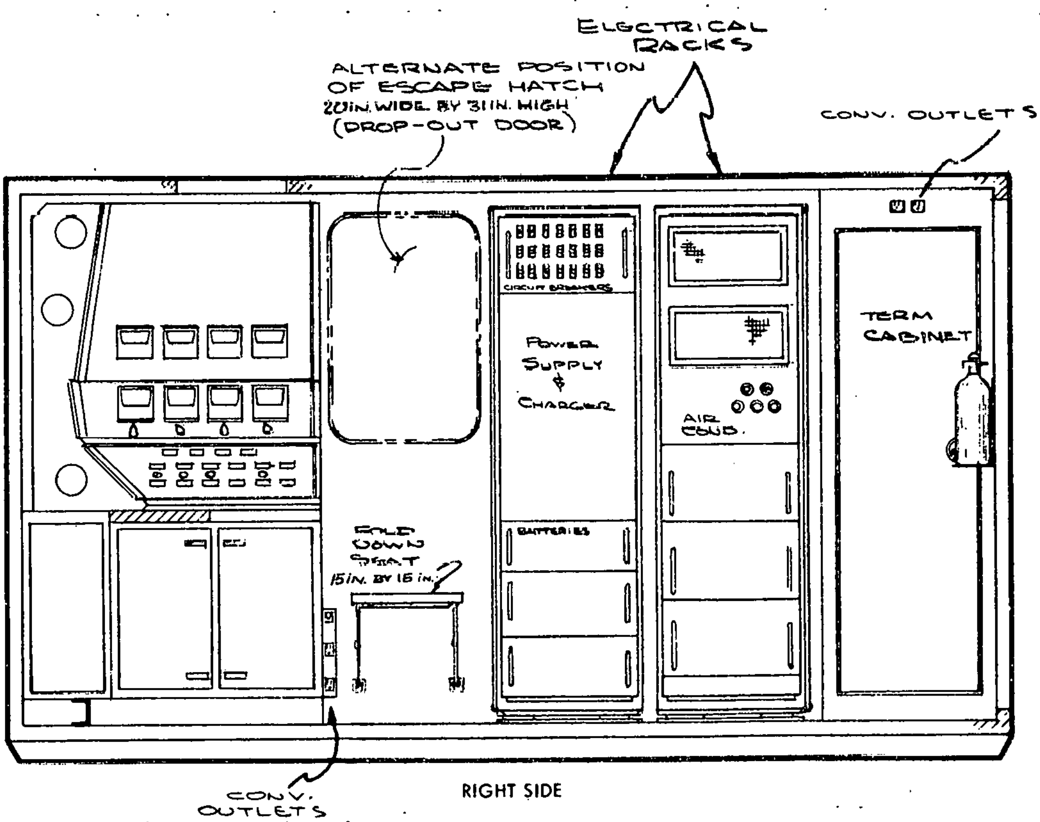


LEFT SIDE



RIGHT SIDE

FIGURE 4-3. ML-1A CONTROL CAB ELEVATIONS

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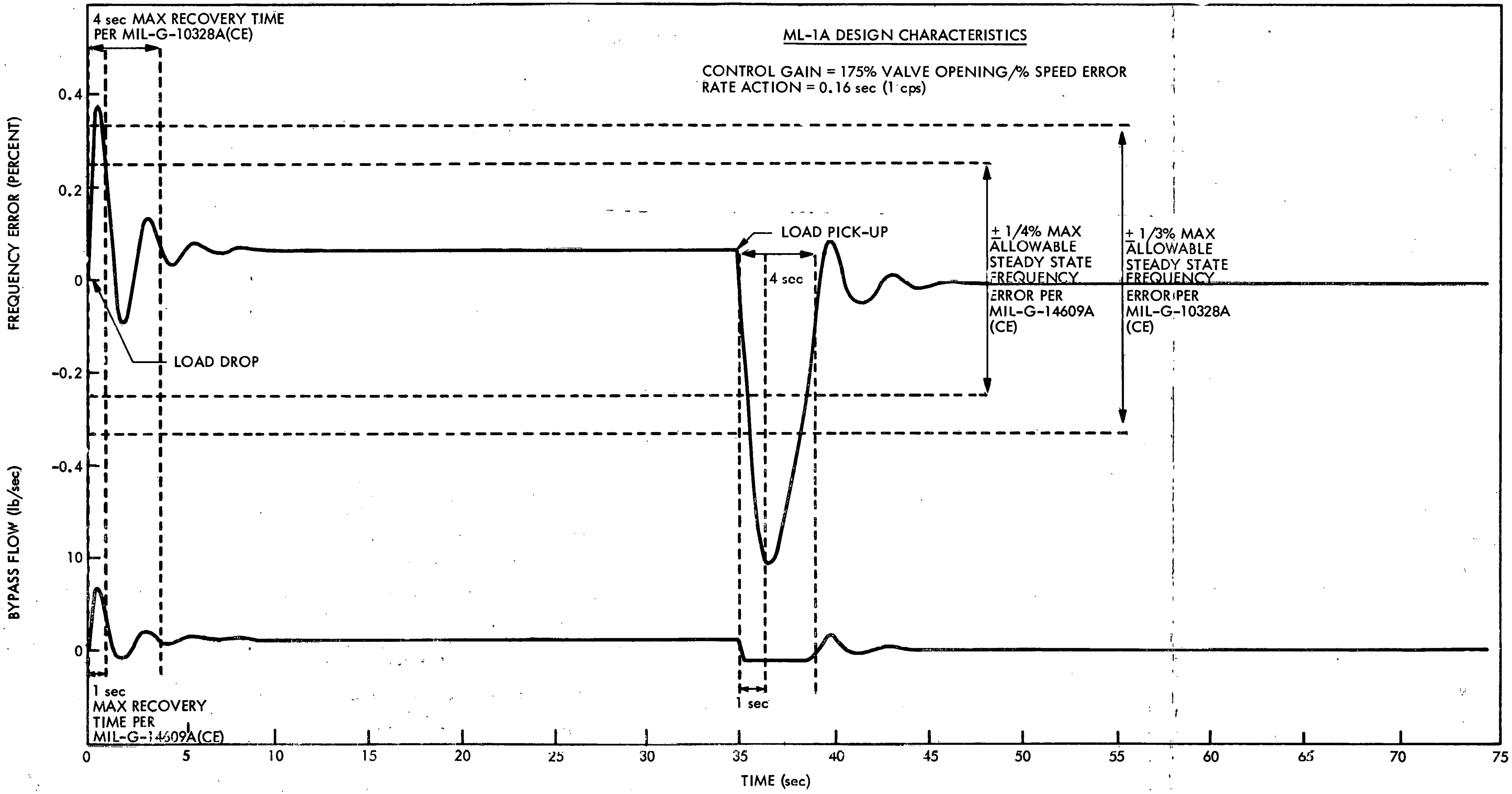

FIGURE 4-4. RESPONSE TO 150 kw LOAD STEPS

- The "desired" speed recovery to ± 1/4% within one second following 50% load steps (per MIL-G-14609A) can be satisfied for load drops, but not for a 50% to 100% step load increase.

- Decreasing the frequency at which the rate, or derivative, network becomes effective from 1 cps to 1/3 cps does not affect the recovery time, but does reduce the number of oscillations before steady state is attained (Fig 4-5).

- It is not possible to design a speed controller to meet the MIL-G-14609A specifications for 50 to 100% step load increases. Since the by-pass valve closes completely immediately following the load increase, the initial speed recovery depends on t-c set characteristics rather than on the controller.

The data discussed above were determined with an analog system which included simulation of the ML-1 3600 rpm alternator. The ML-1A design now contemplates the use of an 1800 rpm alternator (see Section 4.3d). When the inertia characteristics of the new alternator are known, the system response characteristics will be recalculated.

f. Component Selection: Instrument components were selected in conjunction with the instrumentation study and the development of the control cab layout. The following instrumentation systems being developed under the ML-1 Improvements Program will be used without change in ML-1A:

- Bearing temperature chassis
- Speed and temperature controller
- SAM system
- Auxiliary speed chassis
- Integrated process temperature chassis
- Integrated process pressure chassis
- Reactor outlet high-temperature scram chassis

Nuclear instrumentation manufactured by the General Electric Co., is being evaluated for use in the ML-1A. The design of the GE system is similar to that of the Stromberg-Carlson system installed in the ML-1. The major advantages of the GE system are improved packaging and the elimination of source range preamplifiers at the power conversion skid.

Components for meters, relays, circuit breakers, switches, terminal boards, control cab equipment racks and slides, and solid state meter relays were selected during the quarter.

4.3 Power Conversion Skid

a. Skid Repackaging: Repackaging of the power conversion skid equipment to accommodate the commercial alternator (longer than the ML-1 brushless alternator) was studied. Layout drawings were made to determine the feasibility

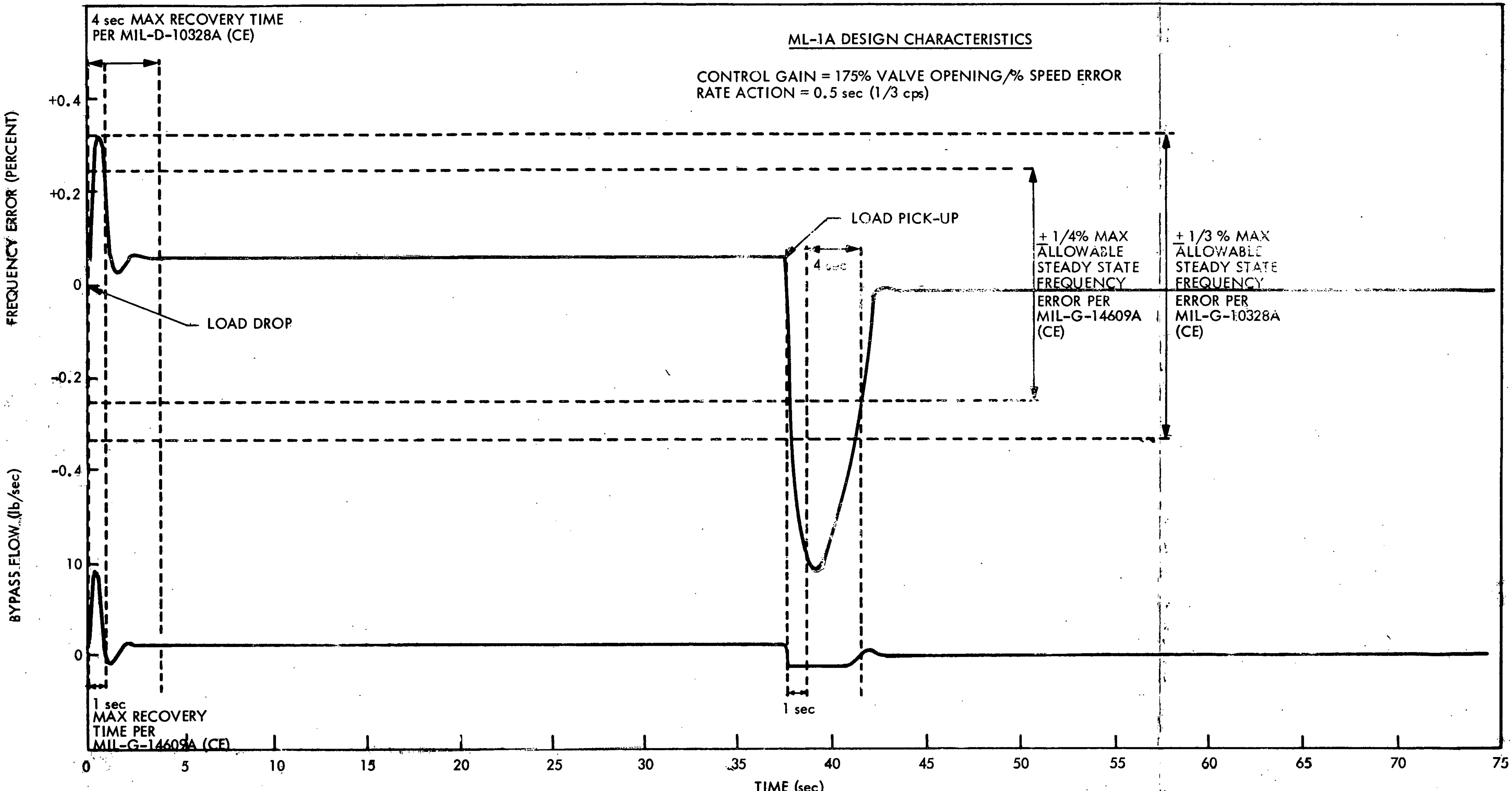

FIGURE 4-5. RESPONSE TO 150 kw LOAD STEPS FOR PROPORTIONAL POSITIVE RATE

11.2-64-306

of relocating the primary coolant gas ducts to move the recuperator as far as possible toward the reactor end of the power conversion skid. This work demonstrated the practibility of moving the recuperator outlet duct to the inside of the reactor shield tank, thus allowing the recuperator to be moved approximately 2 ft. This relocation makes approximately 5 ft available for the alternator within the envelope of the power conversion skid package. The ML-1A power plant layout, showing the location of the major power conversion skid components, is illustrated in Figs 4-6 and 4-7. An analysis of the power conversion skid weight considering the use of different capacity alternators, and the different t-c sets, was completed concurrent with the repackaging study. Table 4-3 summarizes the results of this analysis and indicates the proposed areas for redesign to reduce the power conversion skid weight to the required limit (30,000 lb).

b. Turbine-Compressor Set: A proposed method of performing the turbine-compressor set preliminary design, and of maintaining technical liaison between AGN and t-c set vendors, was developed and forwarded to the USAEC for approval (Ref. 25). A specification control drawing and specification was prepared for the ML-1A t-c set. This specification is similar to those under which the original ML-1 t-c sets were procured, except that the gear set output speed has been specified at 1800 rpm, and the state points have been redefined to be consistent with the ML-1A systems analyses. The requirement for providing an oil pump power takeoff from the gear set has been deleted.

c. Lubrication System: The preliminary design of the ML-1A lubrication system was initiated with the development of system piping and instrumentation diagrams for both the Clark CSN-2 and Stratos 670-2 applications. The differences between the two systems were defined, and design efforts were directed toward minimizing these differences.

The lubricating oil sump tank, which is pressurized in ML-1, will operate at atmospheric pressure on the ML-1A. This is made possible by the specification of a commercial alternator, thus eliminating the alternator pressurization system, contaminated gas return from the alternator, and the sump equalizing compressor. The weight savings achieved with this simplification is about 250 lb. The design of the unpressurized sump tank was initiated. The oil heaters will be mounted in the sump in a manner which facilitates removal and replacement. The heater power density was reduced from 20 w/sq. in. to 5 w/sq. in. to extend the heater element lifetime and to prevent the possibility of oil breakdown because of excessive temperature.

A detailed study to determine the feasibility of eliminating the contaminated seal gas cleanup system refrigeration unit was performed. Several alternative approaches were considered, including the use of an air-cycle refrigeration system or a Hilsch vortex tube. A Freon refrigeration system appears to be the only practical approach. Additional studies were initiated to determine if the desiccant separator was sufficient for cleanup without the requirement for refrigeration.

Reduction of lubricating oil pump power consumption, with special emphasis on startup requirements, was investigated. A preliminary design was developed using constant-flow, variable-pressure type pumps. In this concept, the standby oil pump will deliver only enough oil to satisfy t-c set bearing demands.

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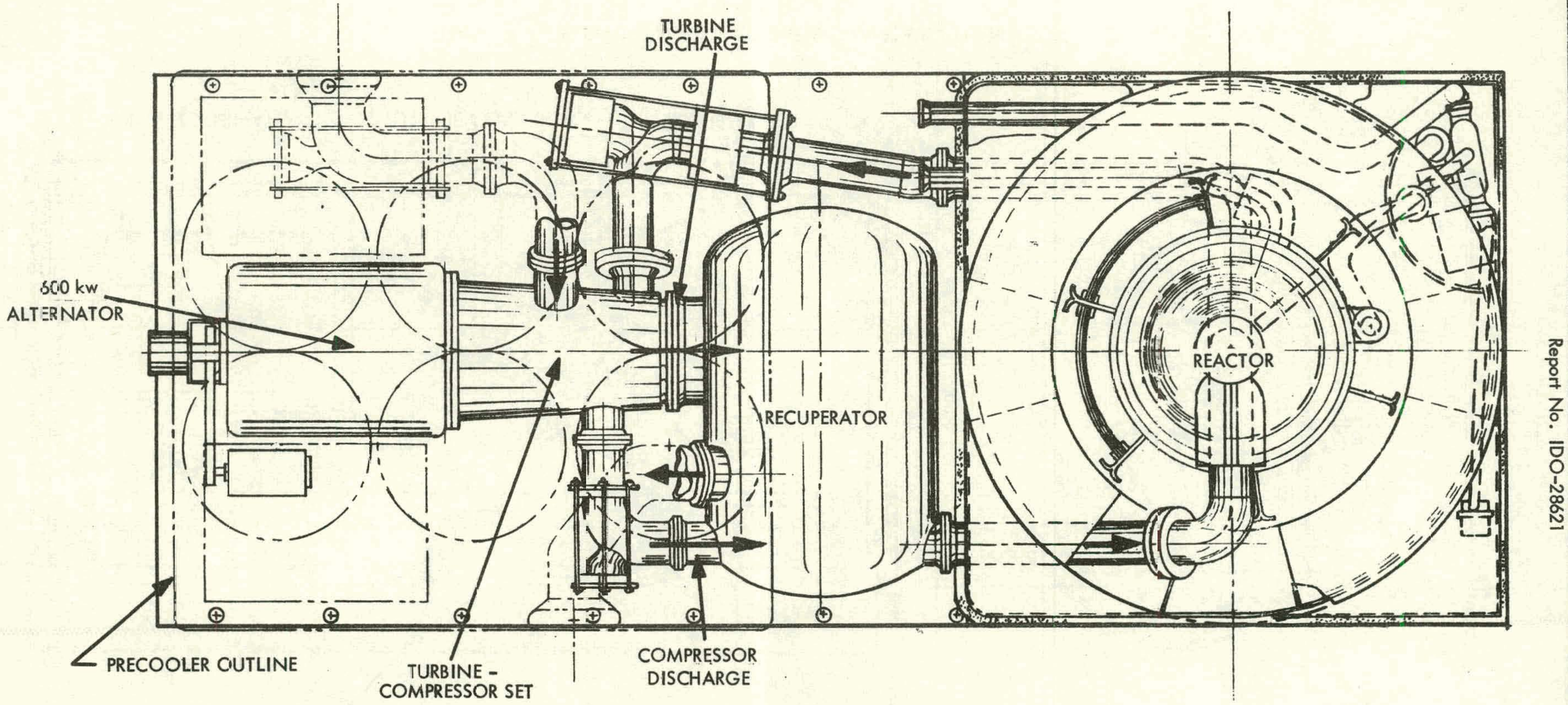
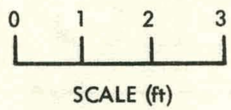


FIGURE 4-6. PLAN VIEW OF THE ML-1A POWER PLANT



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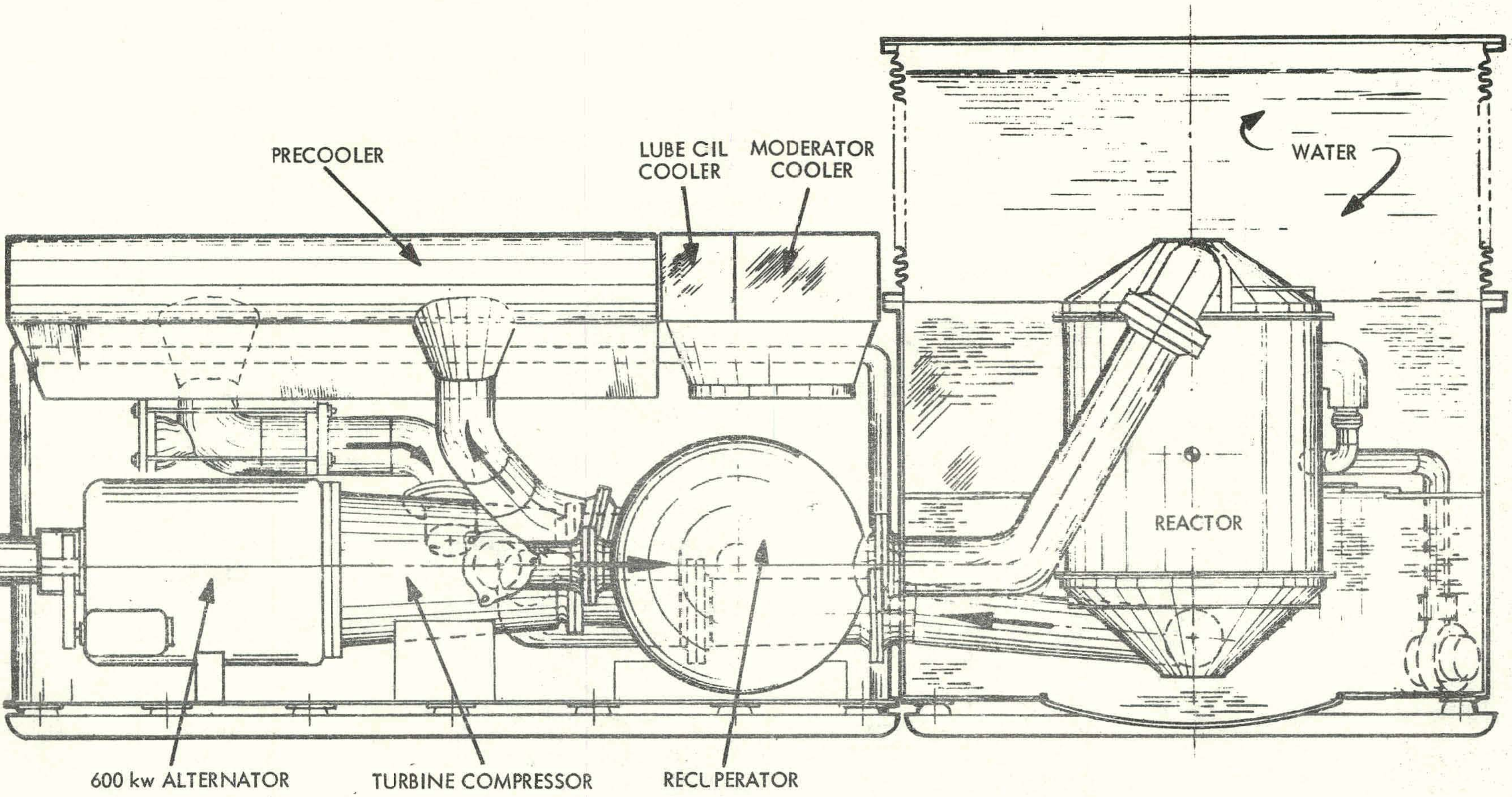

FIGURE 4-7. ELEVATION VIEW OF THE ML-1A POWER PLANT

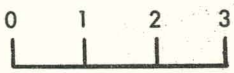

TABLE 4-3 - ML-1A POWER CONVERSION SKID INITIAL WEIGHT SUMMARY (in pounds)

	With Clark CSN-2			With Stratos 670-2		
		ML-1A Skid			ML-1A Skid	
	ML-1 Skid	(300 kw Net)	(500 kw Net)	ML-1 Skid	(300 kw Net)	(500 kw Net)
Turbine-Compressor Set	1,528	1,528	1,528	3,700	3,700	3,700
Recuperator	4,796	4,796	4,796	4,796	4,796	4,796
Precooler	4,013	4,605	4,605	4,013	4,605	4,605
Moderator/Lubrication Cooler	1,515	1,385	1,385	1,515	1,385	1,385
Motors-Fans-Plenums	1,300	1,300	1,300	1,300	1,300	1,300
Alternator (including gear reduction)	5,900	4,500	6,400	5,900	4,500	6,400
Start Motor	360	600	600	360	600	600
Skid Structure	4,900	4,400	4,400	4,900	4,400	4,400
Lubrication System	1,870	1,270	1,270	1,870	1,270	1,270
Electrical System	4,460	3,400	3,400	4,460	3,400	3,400
Primary System Piping	2,589	2,089	2,089	2,589	2,089	2,089
Miscellaneous Piping	841	500	500	841	500	500
TOTAL	34,072	30,373	32,273	36,244	32,545	34,445

Redesign areas to reduce weight:

	Estimated Weight Reduction, pounds
Detailed Weight Optimization Study of Lubrication System	200
Detailed Weight Optimization Study of Electrical System	300
Detailed Weight Optimization Study of Skid Structure	400
Detailed Weight Optimization Study of Piping	200
Reduce Precooler Effectiveness to 92.5%	450
Reduce Precooler Allowable Inlet Temperature to 477°F	600
Remove Electrical Components to Auxiliary Skid (Zero sequence independence 75 KVA transformer, 75 KVA transformer, capacitors and inductors, etc.)	2400
Recuperator Redesign	500
TOTAL	5050

During startup, as the oil flow from the main shaft-driven pump increases, the flow from the standby pump will decrease, thus minimizing the auxiliary power consumption.

d. Precooler: Review of the ML-1 and improved ML-1 precooler designs determined that the existing ML-1 precooler design was unacceptable because of mechanical deficiencies, and that the improved ML-1 precooler design would not satisfy the ML-1A skid envelope specifications. In addition, the systems analysis performed for the ML-1A power plant indicated the desirability of obtaining a precooler effectiveness greater than the 87% demonstrated for the ML-1 design, and the 88% projected for the improved ML-1 design.

The use of a plate-fin type precooler was investigated. This investigation included re-evaluation of studies performed during the initial phases of the ML-1 improved precooler design early in 1963. This work indicated that the plate-fin concept had superior performance characteristics (with respect to the finned tube concept) but that it weighed more than the tube concept. The two concepts were compared to define the performance advantage for the plate-fin design and the effect of this advantage on power plant thermodynamic power. Table 4-4 summarized the significant parameters that influence performance of the two concepts.

TABLE 4-4 - PRECOOLER PERFORMANCE SUMMARY

		Concept	
		ML-1 Improved Finned Tube	Plate-Fin
1.	Effectiveness	88%*	94%**
	a. Primary Heat Transfer Area, sq. ft	2330	7800
	b. Secondary Heat Transfer Area, sq. ft	17,250	32,500
	c. NTU	4.76	6.0
2.	Pressure Drop		
	a. Core, psid	1.80	0.88
	b. Flow Area, sq. ft	1.36	2.74
	c. Velocity, ft/sec	45.5	26.1
	d. N_{Re}	16,300	5,000

* This is a demonstrated number with deficient air-side cooling. The effectiveness is calculated to be 91% when the air flow rate is equal to the design flow rate.

**This is a calculated number with correct air-side cooling.

Inquiries regarding the design and fabrication of a precooler core to meet ML-1A design requirements were sent to several heat exchanger vendors. The engineering aspects of the fabrication were discussed in detail during a visit to the Stewart-Warner plant. (Stewart-Warner submitted a bid for fabrication of the improved ML-1 precooler in the spring of 1963.) This discussion confirmed reasonableness of the information summarized in Table 4-3, and Stewart-Warner's willingness to undertake the fabrication of a plate-fin core for the ML-1A precooler.

A precooler core design based on the primary working fluid state points calculated by the system analysis was completed. Geometry, manifold locations, and mounting locations were specified to be compatible with the relocation of the major components on the power conversion skid required to accommodate the commercial alternator. The outlet and inlet ducts were located at off-center positions with respect to the core to improve the internal flow distribution, thus improving performance and minimizing thermal stresses. Component specifications and drawings were completed and forwarded to the ML-1 Power Conversion Project for procurement.

When bids are received from the potential precooler fabrication vendors, it is planned to procure a core for testing under the ML-1 Improvements Program. If procurement is initiated, the ML-1A precooler design will be based on the successful bid. If no acceptable bids are received, the ML-1A precooler design will require further evaluation.

The design of the precooler air-side cooling arrangement was reviewed. Tests conducted at Aerojet-Azusa on the original ML-1 precoolers indicated that the air-side cooling flow was 84% of the design value of 247,500 lb/hr. Air velocity and flow tests were conducted on the ML-1 precooler at NRTS to confirm this value and to determine in detail the flow distribution and the effect of the location of other power conversion skid equipment on flow distribution. Although the final results of these tests were not available at the end of December, preliminary reduction of the data indicates the validity of the previously determined flow value (84% of design flow).

The detailed design of an improved air-side cooling system was initiated. The design approach was to maintain the basic concept of providing six fans in plenum structures, but to increase the flow capacity and optimize the flow distribution. The modifications include increasing the diameter of the propellers from 28 to 36 in., and decreasing the speed from 3400 to 1725 rpm. The calculated flow for such a design is 255,000 lb/hr at sea level and 100°F. It is anticipated that the larger diameter fans, coupled with the modification to the fan plenum to accommodate the larger fans, will also improve the cooling flow distribution.

A study of the precooler fan drive motor design directed toward minimizing startup power requirements was initiated. A hydraulic motor design is being considered in addition to the existing (ML-1) two-speed electric motor design. The hydraulic system offers the advantages of variable speed capability (minimizing power plant startup power requirements), better air distribution (all fans always rotate at equal speeds) and the capability for automatic control of fan speed and power. Plant weight would be decreased, primarily because the capacity of the auxiliary power transformer could be reduced from 75 kva to about 25 kva. These advantages are being weighed against the unknown reliability and known complexity of the hydraulic approach.

The air-side cooling design being developed will be equally effective with the finned-tube core and fin-plate core; consequently the design is not dependent on a specific approach in the ML-1A precooler design effort.

A detailed technical progress report discussing the ML-1A precooler design was written and published (Ref. 26).

e. Alternator: When the scope of the ML-1A preliminary design was formulated Aerojet and the USAEC agreed that a commercial alternator would be specified for the ML-1A (Ref. 20). It was recognized that a significant relocation of major equipment on the power conversion skid would be required to accommodate commercially available alternators in the 400 to 600 kw range. It was further recognized that the alternator rotating speed would probably be reduced from the ML-1 reference of 3600 to 1800 rpm, resulting in the requirement for a significant redesign of the t-c set gear reduction unit.

Inquiries were sent to 19 electrical machinery manufacturers, describing the ML-1A alternator requirements, and requesting information and specifications for a light-weight 400 kw, 2400/4160 volt, 60 cycle alternator. Eight potential vendors responded to this initial inquiry. Subsequently, Aerojet and the USAEC agreed on the desirability of obtaining an alternator with as high a power rating as possible (up to 500 kw net plant output) to take advantage of the ML-1A power available at ambient temperatures below 100°F. The limiting parameters, in addition to 500 kw net power, were the power conversion skid weight and package size limitations. Accordingly, this requirement was discussed with the responsive vendors and replies for 600 kw units were received from three vendors. In all contacts with these vendors, the ML-1A power quality requirements were stressed and all of the positive responses included this provision.

The power conversion skid repackaging design study was performed concurrently with the market survey discussed above. It was determined that an additional 2 ft for the alternator could be provided by transferring the recuperator-to-reactor gas duct to the reactor skid. An ML-1A alternator specification and specification control drawing were developed on the basis of this information and the information from the market survey. The approach used the basic alternator designs proposed by the various vendors and specified modifications to the housing for mating with the t-c set on one end, and the start motor and lubricating oil pump on the other end. Class H insulation was specified to provide radiation resistance and to permit higher operating temperatures with the accompanying decrease in weight. This specification was in final review at the end of December prior to submission to potential vendors for quotations.

A detailed technical progress report discussing the ML-1A alternator design was published (Ref. 26).

f. 60 kw Startup Power Limitation: Auxiliary power requirements during power plant startup were analyzed to determine the electrical power available for the start motor from the 60 kw auxiliary diesel generator unit. Table 4-5 shows the preliminary evaluation of auxiliary power loads, other than the start motor, that will be required during power plant startup at 100°F ambient conditions.

The power requirement from Table 4-5 at 0.8 power factor is 53.5 kw. It is anticipated that the indicated power requirements can be reduced by more efficient use of the heat exchanger fans, standby oil pump and moderator pump;

and by determination of actual demand factors. The design goal was established as 45 kw for all auxiliary power startup loads other than the start motor, thus leaving 15 kw available for rotating the t-c set.

TABLE 4-5 - ESTIMATED ML-1A STARTUP AUXILIARY POWER REQUIREMENTS*

Component	Load, kva
Control cab	5.0
Moderator pump	10.7
Shield water pump	1.9
Startup compressor	8.4
Standby oil pump	10.7
Oil separator refrigerator	1.0
Precooler fans (No. 1 bank only)	25.2
Moderator/lubrication cooler fans (No. 1 bank, 1/2 speed)	4.0
Total	66.9

*Exclusive of start motor

Four start motor concepts were evaluated. The weights of these design concepts are shown in Table 4-6.

TABLE 4-6 - ML-1A START MOTOR CONCEPTS

Concept	Total Weight, lb
ML-1 start motor	544
Single-speed ac motor	
"Brute force" concept*	1167
Transient startup concept**	500
Variable-speed dc motor	620
Hydraulic motor system***	615

* The "brute force" concept is defined as conventional use of a single-speed ac motor sized correctly for acceleration torque requirements, but oversize for constant speed operation.

** The "transient startup" concept involves coordination of reactor power increases with start motor operation to minimize acceleration loads on the motor.

***A constant speed electric motor driving a constant speed hydraulic pump coupled to a variable speed hydraulic motor.

Evaluation of the four potential startup systems indicated that either the variable-speed dc motor or the motor-driven hydraulic system has promise of being capable of starting the plant with the 15 kw of available power.

Preliminary design layout drawings of both concepts were completed. The installation of the two systems is quite similar: A drive belt transmits dc motor or hydraulic motor power to the alternator shaft; a mechanical override clutch on the alternator shaft permits the shutdown of the start motor system when the power plant reaches self-sustained operation but automatically re-engages the alternator shaft if start motor power is required thereafter. This feature will prolong the life of the startup system, including the drive belt, and generally improve plant reliability. The assembly is designed for easy maintenance. All components used in either design are standard items of proven reliability.

Start motor concepts were being further evaluated at the end of December. A detailed technical progress report discussing the ML-1A start motor design was published (Ref. 26).

g. Systems Analysis: The ML-1A preliminary energy balance and systems analysis was submitted in September 1963 (Ref. 27). This report presented the results of the initial systems evaluation of the ML-1A power plant. Turbine-compressor set, precooler, recuperator, and reactor performance were discussed and the derivation of the values used for the initial ML-1A analysis shown. The thermodynamic power was calculated by applying influence coefficients to the ML-1 reference systems analysis.

Discussions with the USAEC in October 1963 indicated that ARM-TEB personnel had raised questions with respect to the validity of the use of influence coefficients and the resultant system thermodynamic power derived in this manner. ARM-TEB personnel were concerned about the ability of the ML-1A system to generate thermodynamic sufficient power on a 100°F day to produce the required 300 kw net electrical power. It was agreed that Aerojet would prepare a revised analysis of the ML-1A system and present it in the format suggested by TEB (Ref. 21).

Systems analysis calculations were performed for the ML-1A using the CHOP computer code, which provides for iteration until a complete balance across the system is obtained (Ref. f). Performance curves were developed for each major system component, permitting iteration around the design point to reflect "real" component performance related to specific system conditions.

The ML-1A preliminary design effort resulted in several component changes which invalidate some initial component performance characteristics. The t-c set performance has been studied in more detail and the reference mixed-mean fuel element outlet temperature has been reduced to 1225°F. The precooler effectiveness has been re-specified in anticipation of the use of a plate-fin core design. The reactor and power conversion skid piping arrangement has undergone major revision to provide space on the power conversion skid for a commercial alternator. With the selection of an 1800 rpm commercial alternator, the speed reduction system must be changed, resulting in potentially higher parasitic losses in the gearbox.

The current reference ML-1A systems analysis, using downgraded t-c set and precooler performance characteristics and a reactor outlet temperature of 1225°F is summarized in Table 4-7.

TABLE 4-7 - ML-1A SYSTEM PERFORMANCE SUMMARY

Net thermodynamic power	449.00 kw
Turbine-compressor set parasitic losses	-39.14 kw
Gross shaft power	409.86 kw
Alternator efficiency	x 94%
Gross electrical power	385.27 kw
Auxiliary power requirements	-60.00 kw
Net electrical power	325.27 kw

Revision of the systems analysis was in progress at the end of December to reflect the performance of the TCS-670-2 and the CSN-2 as given in the design reports for these units.*

4.4 Fuel Element Development

It was stipulated at the initiation of the ML-1A preliminary design that the ML-1-I fuel element design would be used without change, except that the burnable poison design would permit the core to operate 10,000 hr without reshimming and the silver reactivity shims would be incorporated in the insulation package (Ref. 28). This design approach was selected initially to make maximum use of the design already demonstrated in the ML-1 test program, to satisfy core lifetime requirements, and to reduce the silver contamination problem observed in the ML-1 testing at NRTS. It was anticipated that the europium burnable poison design being developed for the ML-1-II fuel element design could be used to provide the required 10,000 hr lifetime.

At the same time, the ML-1A pressure vessel design required an increase in the pressure tube wall thickness from 0.020 to 0.030 in., and a change in the pressure tube material from AISI Type 321 stainless steel to Type 347 stainless steel. Calculations of the effects of these changes on the system reactivity showed that:

- The addition of 0.010 inches of stainless steel to the inside of the Type 321 pressure tubes would decrease reactivity by about 0.7% $\Delta K/K$.

- The addition of the same 0.010 inches of stainless steel to the outside of the Type 321 pressure tubes would decrease reactivity by about 1.5% $\Delta K/K$. The increased loss of reactivity is due to moderator water displacement.

*Clark: Design of a Closed Cycle Gas Turbine Compressor Set for ML-1, UD-198, Clark Brothers, Olean, New York, 15 October 1962, Contract DA-44-009-ENG-5152.

Stratos: Stratos Model TCS-670-2 Turbine-Compressor Set for ML-1 Closed Cycle Gas Turbine Power Plant, SR-395, Stratos Division of the Fairchild-Stratos Corp., 15 May 1963; Contract DA-44-009-AMC-115(X).

- A change from AISI Type 321 stainless steel to Type 347 stainless steel (with 0.01% cobalt) in the pressure tubes would decrease reactivity by less than 0.02% Δ K/K.

The results of preliminary estimates to determine the excess reactivity available in the ML-1-I nuclear design with europium burnable poison and 0.030-in. thick AISI Type 347 stainless steel pressure tubes are summarized in Table 4-8.

TABLE 4-8 - EXCESS REACTIVITY OF ML-1A CORE

ML-1-I k_{eff}, cold, clean, no shims, no burnable poison		1.067 Δ K/K
Required k_{eff} for ML-1A:		
Critical	1.000 Δ K/K	
Fuel burnup, fission products	0.023 Δ K/K	
Xe, moderator ΔT, pressure, fuel ΔT	0.002 Δ K/K	
Power adjustment	0.002 Δ K/K	
Control	0.007 Δ K/K	
Total Reactivity Requirement		1.034 Δ K/K
Available excess reactivity		0.033 Δ K/K
Disposition of excess reactivity:		
In europium at 10,000 hours	0.019 Δ K/K	
Tube wall thickness change	0.007 Δ K/K	
Total		0.026 Δ K/K
Net Available Excess Reactivity		0.007 Δ K/K

Only 0.007 Δ K/K is available to compensate for manufacturing tolerances and to be absorbed in reactivity shims. This value is marginal compared with the 0.045 Δ K/K available in the ML-1. The small margin also minimizes the use of reactivity shims which, in addition to absorbing excess reactivity, flatten the radial power distribution. If very few or no shims are used, the power, and the hot spot temperature, in the center elements will be higher than those in ML-1.

Based on the above analysis, it was concluded that the ML-1-I design, with europium for a 10,000-hr burnable poison, was unacceptable for ML-1A because of the small amount of excess reactivity available. It appeared that the only acceptable approach would be to use the ML-1-II nuclear design. This was considered to be undesirable because the ML-1-II core performance cannot be demonstrated on a schedule compatible with the ML-1A design effort.

More detailed reactivity and lifetime calculations were performed for both the first and second core designs, using different amounts of both cadmium and europium for burnable poison. The calculations used the two-group perturbation methods developed for the ML-1 lifetime analysis. Minor modifications were made to incorporate new data for high-energy fission product cross sections, and to make use of additional work done on the change of intra-cell fast flux shapes during lifetime. Results of these calculations indicated the following:

- A 10,000 hour lifetime, with adequate excess reactivity, is attainable with the ML-1-I design when cadmium is used for the burnable poison. In essence, some of the silver reactivity shims now in the ML-1-I would be left out, and more cadmium provided in each element. With this approach, the reactivity variation over the 10,000 hour lifetime would be 1% $\Delta K/K$, reflecting the faster burnup characteristics of cadmium. An additional 2.2% $\Delta K/K$ excess reactivity (above the burnable poison and burnup requirements) exists which would compensate for manufacturing tolerances and calculational errors. This equivalent amount of reactivity, in the form of reactivity shims, would be used in the core.

- The ML-1-II design, with europium as the burnable poison, results in a variation of only 0.25% $\Delta K/K$ over the 10,000 hour lifetime and a calculated 3.3% excess reactivity to correct for calculational errors and manufacturing tolerances.

As a result of this study, it was concluded that the ML-1-II design offered the advantages of smaller reactivity variation over lifetime; the ability to operate 4,000 or 5,000 hr beyond the 10,000-hr lifetime without serious additional loss of reactivity; and a smaller fuel loading. The ML-1-II design, however, is undesirable for the ML-1A first core because it has not been fabricated and operated. On the other hand, the ML-1-I approach is almost an exact neutronic copy of the present ML-1 core, which has been built and tested. The ML-1-I approach has therefore been selected as the reference design for ML-1A. Table 4-9 presents the reactivity balances for the two designs discussed above.

Calculations were made to determine the flooded shutdown margin of the ML-1A design over 10,000 hours, when the system reactivity will rise 1% $\Delta K/K$ due to cadmium burnup. Using ML-1 experimental data, and extending it to the ML-1A design, it was determined that the ML-1A design will be sub-critical in the flooded condition with at least two shim-safety rods withdrawn.

At the end of the report period, the determination of the power distribution over the core lifetime was in progress.

TABLE 4-9 - REACTIVITY BALANCES FOR ML-1-I and ML-1-II CORES

		ML-1-I		ML-1-II	
I.	**AVAILABLE REACTIVITY**				
	Designed excess; cold, clean weight percent shims or poison	6.70%		10.60%	
	Less worth of extra 0.010 in. thick pressure tube wall	-0.70		-0.70	
	Available designed reactivity		6.00%		9.90%
II.	**REACTIVITY REQUIRED FOR STARTUP**				
	Equilibrium xenon worth, 3.4 Mw	0.89%		1.02%	
	Xenon over-ride	0.00		0.00	
	Coolant gas pressure	0.11		0.11	
	Excess for 20-sec reactor period	0.20		0.20	
	Total start-up requirements		-1.20		-1.33
III.	**REACTIVITY REQUIRED FOR BURNUP**				
	Fuel burnup	1.52%		1.71%	
	Non-saturating fission products	0.33		0.44	
	Slowly-saturating fission products	0.76		0.83	
	Total burnup requirements		-2.61		-2.98
	Reactivity available for shims or remaining in burnable poison at end of lifetime				
	(I-II-III)		2.19%		5.59%

IV. GCRE MODIFICATION

5.0 TITLE II DESIGN

After discussions with the USAEC, it was agreed to design modifications to the GCRE facility to permit test operation of the ML-1 reactor skid in that facility. The design effort was initiated 1 November 1963 and was in process at the end of December. The estimated completion date is 31 January 1964.

The major tasks initiated during the report period were:

- The design of gas ducts to connect the reactor skid to the existing GCRE ducting
- The design for the relocation of the existing GCRE pool water heat exchanger to the floor of the reactor pit to serve as a moderator cooler for the ML-1 reactor skid
- The design of supplemental shielding, including a tank to be placed over the reactor and filled with borated water, and concrete shielding to be placed over the reactor pit
- The design of a ventilation system for the reactor pit
- The design of electrical modifications to accommodate the operation of the ML-1 reactor skid
- The design of modifications to the ML-1 and GCRE instrumentation and controls to permit the safe operation of the ML-1 reactor skid
- The preparation of specifications and drawings describing the work
- The preparation of a safety analysis of the operation of the ML-1 reactor skid in the modified GCRE facility

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APPENDIX A

ML-1 PLANT CHARACTERISTICS

1. GENERAL

Design performance at 100°F		
Gross electrical output	400 kw	
Net electrical output	330 kw	
Reactor thermal power	2.9 Mw to gas; 3.3 Mw total	
Cycle efficiency	13.3%	
Plant thermal efficiency	10%	
Coolant flow	92,000 lb/hr	
Dose rate at control cab @ 500-ft during full power operation	5 mr/hr (with expedient shielding as needed)	
Dose rate at 25 ft, 24 hr after shutdown (direction of transport vehicle driver with P-C skid in place)	15 mr/hr	
Overall plant dimensions	279 x 113 x 93 in. high	
Overall plant weight and dimensions	Weight	Dimensions (in.)
Reactor package	30,000 lb	111 x 110 x 93 high (plus ion exchange column on end)
Power conversion package	30,000 lb	168 x 113 x 93 high
Control cab	6500 lb	145 x 82 x 81 high
Auxiliary equipment	15,000 lb	- - - - - - - - -
Operating supplies (startup and 90 day operation):		
Demineralized water	2900 gal	
Nitrogen (with 0.5 vol% oxygen)	2400 scf	
Oxygen	200 scf	

Anhydrous boric acid (B_2O_3)	1200 lb
Mixed bed ion exchange resin	900 lb max.
Lubricating oil	60 gal
Filter elements	7
Plant startup time	12 hr
Auxiliary power requirements	
Pre-startup	30 kw max.
Normal startup	75 kw max.
Normal shutdown	45 kw max., 3 kw ave
Emergency shutdown	none
Reactor drying	36 kw max.

2. REACTOR THERMAL CHARACTERISTICS

Power density	700 kw/ft^2
Maximum heat flux	140,500 $Btu/hr/ft^2$
Average heat flux	78,200 $Btu/hr/ft^2$
Heat transfer surface	126.5 ft^2
Maximum to average heat flux ratio	
Axial	1.5
Radial	1.2
Maximum fuel center temperature (including hot spot factors)	$2160^{\circ}F$ ($BeO-UO_2$) $2650^{\circ}F$ (UO_2)
Maximum moderator temperature	$190^{\circ}F$
Maximum surface temperature of fuel cladding (nominal, average)	$1520^{\circ}F$
Maximum surface temperature of fuel cladding (including hot spot factors), reference	$1750^{\circ}F$

3. REACTOR NUCLEAR CHARACTERISTICS

Average thermal neutron flux (fuel)	1.9×10^{12} $neut/cm^2$-sec
Average fast neutron flux (fuel)	1.7×10^{13} $neut/cm^2$-sec
Maximum to average thermal flux ratio	3.9
Hydrogen to U-235 atom ratio	40
Core buckling	0.0059 cm^{-2}
Fermi age	60 cm^2
Square of thermal diffusion length, L^2	2.05 cm^2
Thermal utilization, f	0.75

Infinite multiplication factor, k	1.54
Neutron lifetime	1.9×10^{-5} sec
k_{eff}, cold, clean core; no shims or burnable poison	1.067
Operating k_{eff}, cold, clean core, with shims and burnable poison	1.014
Core life, full power	3000 hr min; 10,000 hr design
Burnup (U-235), average	3.6% in 10,000 hr
Maximum	6.5%
Prompt temperature coefficient, $\Delta k/k$-°C	
at 0°C	$+0.3 \times 10^{-6}$
at 90°C	-0.5×10^{-6}

4. REACTOR VESSEL

Materials	
Tube sheet	Stainless Steel, Type 304, 2.94 in. thick
Pressure tubes	Stainless steel, Type 321
Source tube	Stainless steel, Type 321
Gas ducts, plenums	Stainless steels, Types 304-L, 321 and 347
Baffle*	Stainless steel, Type 321; Tungsten; and Inconel X (springs)*
Outside diameter	30.960 in. max. (exclusive of upper flanged connection)
Overall height	79.5 in.
Pressure tube length	24 in. between inside surfaces of tube sheets
Design pressure	345 psia (gas)
Design temperature	525°F (max.)*
Wall thicknesses	Tubes 0.020 in. Plenum 2.12 in. min
Source tube	0.020 in. wall thickness; 0.500 in. OD

5. REFLECTOR

Composition, top	2 in. H_2O; 4.5 - 5.0 in. stainless steel; 1.5 in. W
bottom	3-4 in. stainless steel; 3 in. W

radial	1.8 in. Pb; 2 in. W; 180^{o} segment 4 in. Pb; 180^{o} segment
Total heat generation	6×10^5 Btu/hr
Maximum power density	360 Btu/hr-in.^{3}

6. BIOLOGICAL SHIELDING

Composition	3-1/2 to 4 in. lead and tungsten plus 30 in. of borated water (10 wt% boric acid)

7. CORE (EXCLUDING REFLECTOR)

Diameter	22 in. equivalent
Height	22 in.
Number of fuel elements	61
Number of coolant passages	61
Number of coolant passes	1
Type of geometry of fuel elements	Cluster of 19 pins (18 fueled)
Cold, clean critical mass, U-235 no shims, no burnable poison	37 kg
U-235 loading	49 kg
Enrichment, inner 6 pins	93% U-235 as UO_2
outer 12 pins	31 vol% UO_2, 93% enriched U-235 69 vol% BeO

Core composition

Materials	Volume %
UO_2	4.3
BeO	3.3
Stainless steel	3.6
Hastelloy X	7.0
H_2O	58.6
Insulation	7.0
Gas void	16.2
Total	100.0

8. FUEL ELEMENT

Dimensions	1.72 in. OD x 32 in.
Fuel material	BeO-UO_2 (outer pins); UO_2 (inner pins)

Number of pins per element	19 (12 w/BeO-UO_2; 6 w/UO_2; 1 empty)
Pin outside diameter	0.241 in.
Pin cladding material	Hastelloy X
Pin cladding wall thickness	0.030 in.
Pin spacer	0.040 in. OD Hastelloy wire
Heat transfer material (pin internal)	He
Pellet diameter	0.176 in. (nominal)
Type burnable poison	Cadmium
Reactivity worth of burnable poison	0.6% at startup

9. CONTROL ELEMENTS

Type	Tapered blades
Location	Moderator
Number: Shim blades	3 pairs (3 actuators)
Safety blades	2 pairs (2 actuators)
Regulating blades	1 pair (1 actuator)
Absorber material: Safety and shim blades	5 wt% Cadmium- 15 wt% Indium- 80 wt% Silver
Dimensions (each blade)	4 x 10.5 x 0.25 to 0.62 in.
Regulating blades	Stainless steel
Dimensions (each blade)	4 x 9 x 0.25 to 0.62 in.
Cladding material	none
Reactivity worth of control elements:	
Safety and shim blades	0.058 $\Delta k/k$
Regulating blades	0.004 $\Delta k/k$
Total	0.062 $\Delta k/k$
Actuating time for regulating blade:	
Drive	13.3 sec for full insertion or withdrawal
Scram	0.35 sec (max.) for full insertion from signal*
Safety and shim actuator:	
Drive	4.0 min for full insertion or withdrawal
Scram	0.35 sec (max.) for full insertion from scram signal*

10. MODERATOR

Type	Water
Reactor inlet temperature	180°F
Reactor outlet temperature	190°F
Pressure	30 psi max.
Flow rate	300 gpm
Type of flow circulation	Forced
Purity:	
Total solids	1 ppm
Resistivity	10^5 to 10^6 ohm-cm
Total heat removal rate	1.5×10^6 Btu/hr

11. REACTOR WORKING FLUID FLOW

Working fluid	99.5 vol% N_2 + 0.5 vol% O_2
Reactor inlet temperature	800°F nominal
Reactor mixed-mean outlet temperature	1200°F max.
Average velocity in core	160 ft/sec
Maximum velocity	180 ft/sec
Inlet pressure	315 psia (max.)
Core ΔP	15 psi
Reactor ΔP	22 psi

12. POWER CYCLE

Type	Brayton cycle with regeneration
Total volume of working fluid system	120 ft^3
Total system working fluid inventory full load at 100°F	52 lb
Working fluid transit time	2.0 sec

Cycle characteristics:

Ambient temperature	100°F	0°F	-65°F
Net power, kw	330	330	330
Reactor inlet, °F	791	597	597
Turbine inlet, °F	1200	990	990
Compressor inlet, °F	132	24	24
Compressor inlet, psia	117	93	93
Compressor outlet, psia	320	294	294
Reactor inlet, psia	313	288	288

13. TURBINE-COMPRESSOR SET

	Stratos t-c Set	Clark t-c Set
Speed, rpm	18,338	22,000
Turbine stages	2	2
Turbine rotor material	Incoloy 901	Incoloy 901
Turbine blade material	Inco 713	N 155
Turbine stator blade material	Inconel	N 155 or 19-9DL
Expansion ratio	2.38	2.38
Compressor stages	2	11
Compressor material	Al 355 T71	403 stainless steel
Rotor shaft	SAE 4340	SAE 4340
Compressor ratio	2.72	2.72
Case material	304 stainless steel	304 stainless steel
Seals		
at journals	Buffered labyrinth	Buffered labyrinth
interstage	Plain labyrinth	Plain labyrinth
shaft	Buffered labyrinth	Double "L" ring seal oil buffered
Bearings		
journal	Tilting pad	Plain babbitt
thrust	Kingsbury type	Kingsbury type (in low press. area)
Support	Overhung turbine	Turbine and compressor supported between bearings

14. ALTERNATOR

Output	60 Cycle Operation	50 Cycle Operation
Rating	500 KVA 3 Ø	417 KVA 3 Ø
Voltage	2400/4160 V	2000/3467 V
Rotor shaft speed	3600 rpm	3000 rpm

Case

Diameter, maximum	40.25 in.
Length, without starting motor	30 in.
Length, with starting motor	35.5 in.

Weight, alternator only	5900 lb
Starting motor	400 lb
Temperature, operating (hot spot)	300°F internal max.

15. RECUPERATOR

Length (including insulation)	81 in.
Outside diameter (including insulation)	49.25 in.
Headers	
High pressure inlet	8 in.
High pressure outlet	8 in.
Low pressure inlet	20 in.
Low pressure outlet	14 in.
Effectiveness	79%
Pressure loss	
High pressure $\Delta p/p$	2.5%
Low pressure $\Delta p/p$	0.85%
Type	Shell and tube regenerator
Tubes	4 passes x 840 tubes
Shell	1 pass
Surface	External fins
Materials	300 series stainless steel

16. PRE-COOLER, MODERATOR COOLER AND OIL COOLER ASSEMBLY

Dimensions:	
Length, overall	166 15/16 in.
Pre-cooler	122 5/16 in.
Moderator cooler	32 1/8 in.
Oil cooler	11 5/16 in.
Width	113 in.
Thickness, overall	32 in.
Core	15 in.
Fans and plenums	17 in.
Materials	
Tubes and fins	Series 1100 aluminum
Headers	Series 2219 aluminum
Weight	6500 lb

Pre-cooler:	
Header, inlet	One, 14 in.
Header, outlet	One, 10 in.
Effectiveness	90%
Total $\Delta p/p^{i}$	3.25%
Air flow	247,500 lb/hr
Type	Fin fan air-to-gas exchanger
Tubes	1105 tubes, single pass
Surface	Internal and external fins
Moderator cooler:	
Headers, inlet and outlet	4 in.
Total Δp	2.77 psi
Water temperature	
In	190°F
Out	180°F
Airflow	73,250 lb/hr
Type	Fin fan air-to-water exchanger
Tubes	88 tubes per pass, three passes
Surface	External fins
Oil cooler:	
Connections, inlet and outlet	1 1/2 in.
Total Δp	9.38 psi
Oil temperature	
In	180°F
Out	150°F
Oil flow	18,900 lb/hr
Air flow	27,500 lb/hr
Type	Fin fan air-to-oil exchanger
Tubes	45 tubes, 2 passes
Surface	Internal and external fins

Report No. IDO-28621

LEGAL NOTICE

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The information contained herein is regarded as preliminary and subject to further checking, verification, and analysis

AEROJET-GENERAL NUCLEONICS