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ARMY GAS-COOLED REACTOR SYSTEMS PROGRAM

FINAL REPORT

FULL POWER AND LIMITED ENDURANCE TEST

OF THE

ML-1 NUCLEAR POWER PLANT

(ANSOP 16625A)

Published

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(ANSOP 16625A)*

ABSTRACT

The results of performance analyses of the ML-1 nuclear power plant based on the test data obtained during ANSOP 16625A, "The ML-1 Full Power and Limited Endurance Test" are presented. The test was accomplished during the period of 13 April through 29 May 1964 at the National Reactor Testing Station in Idaho. During this period, the reactor operated for 1493.35 Mw-hr at an average power of about 2.2 Mw(t), and 823.28 hr of turbine-compressor set rotation were logged, including 664.55 hr at self-sustained conditions. All major objectives of the test were achieved.

Analysis of the performance data demonstrated a significant advance in the development of the power plant. A number of startup and shutdown transients were experienced during the test but no major problems developed. The primary performance deficiency observed is associated with the turbine-compressor set; the maximum measured net power of 139 kw is considerably less than the 234 kw predicted for corresponding operating conditions. The mechanical and neutronic performance of the reactor was essentially as predicted. The analyses of the effectiveness of the reactor shielding indicated that this critical part of the plant performed as designed. The performance of other components was generally acceptable and very similar to that experienced in earlier testing. The overall power output of the plant was less than predicted, primarily because of the poorer than anticipated performance of the turbine-compressor set.

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I. INTRODUCTION

The ML-1 mobile nuclear power plant, developed under the Army Gas-Cooled Reactor Systems Program (AGCRSP), consists of a high temperature gas-cooled, water-moderated reactor coupled with compact power conversion equipment. The closed-cycle, gas turbine power plant has a design power output of 300 to 500 kw(e), depending on the ambient temperature. The ML-1 is a demonstration unit; field plants (ML-1A) designed to furnish electrical power at remote military installations will ultimately be based on this design. Additional information concerning the AGCRSP and the ML-1 is presented in Appendix A.

The construction of the ML-1 power plant was completed at the National Reactor Testing Station (NRTS) in June 1962. Following a detailed checkout, the initial self-sustained operation of the plant (ANSOP 16615) was accomplished in September 1962 (Ref. 1)*. During this test, the plant produced 50 kw of power and the maximum reactor power attained was 1.6 Mw. The reactor power was limited by high indicated fuel element and tube sheet temperatures. It was determined that these high temperatures were the results of significant bypass gas flow around the fuel elements. The fuel element seating concept was modified after the test to correct this deficiency. The initial full power and limited endurance tests (ANSOP 16625) were accomplished during February and March 1963 (Refs. a and 2). During these tests, the plant delivered a maximum of 251.5 kw(e) at 95 psia and 2.94 Mw(t) reactor power at an ambient temperature of 16°F. Approximately 100 hr of endurance operation were accumulated at a reactor power level of 2.2 Mw(t). The maximum reactor power level attained was 3.17 Mw(t).

*References are listed after the main body of text. Alphabetical designations refer to reports which are generally available, and numerical designations to in-contract documents.

During the latter phase of the above tests, an apparent increase in the turbine and compressor bearing oil flow rates was noted. Analysis of oil samples revealed significant quantities of tin, indicating a higher than expected wear rate in the bearings. Detailed inspection of the bearings after the scheduled shutdown revealed substantial increases in bearing clearances although visual inspection indicated that the bearings were in excellent condition. Subsequent evaluation indicated that the wear was the result of instability in the bearing/rotor system. A development program was undertaken to increase bearing lifetime by stabilizing the bearing/rotor system. This program evolved a pressure-dam type bearing which showed considerable promise of achieving the design 10,000-hr lifetime. At the same time, the CSN-1 turbine-compressor (t-c) set turbine nozzle was modified to reduce the flow area by approximately 13%. This modification was designed to improve the aerothermodynamic match between the turbine and the compressor and thus improve the performance of the machine. The modified t-c set, designated the CSN-1A, was shipped to NRTS in early March 1964.

It was also determined after ANSOP 16625 that the reactor pressure vessel had developed a small leak which permitted gas flow from the primary coolant system into the moderator water system. This leak apparently did not develop until at least 8 hr after the shutdown which terminated the test. The reactor pressure vessel was removed from the shield assembly and the leak was located and repaired (Ref. b). Concurrent with this work, the fuel elements were modified by the replacement of lower spider and orifice units (Ref. 3) and new reactivity shims were installed in the reactor core (Ref. 4).

II. SUMMARY

The "ML-1 Full Power and Limited Endurance Test (ANSOP 16625A)" was initiated on 13 April and terminated on 29 May 1964. During this period, the plant experienced eight shutdowns: five scrams occurred as a result of instrument malfunctions; the plant was shutdown under normal procedures twice for equipment maintenance; and the shutdown which terminated the test resulted from failure of cable insulation which caused a power failure. All major test objectives were accomplished and a total of 823.28 hr of t-c set rotation were accumulated, including 664.55 hr at self-run conditions. The reactor accumulated 1493.35 Mw-hr.

The performance of the ML-1 power plant during this test was significantly improved over that observed in earlier tests, generally in the area of mechanical performance. The extensive thermodynamic data obtained permitted a more meaningful evaluation of the power plant and component performance, and the relatively long operating time at significant reactor power levels made possible good evaluations of shielding performance and long-term reactor reactivity changes. The excessive t-c set bearing wear and the reactor pressure vessel leak experienced during ANSOP 16625 were not experienced during this test. The demonstrated capacity of the plant to continue operation after several interruptions indicated a significant advance in the state of development of the equipment.

The overall plant thermodynamic performance was significantly poorer than predicted. The predicted net power output at 1150°F turbine inlet temperature, 55°F ambient temperature and 90 psia compressor inlet pressure was 234 kw. The actual net power output at the above conditions was 139 kw. The precise reasons for this performance deficiency have not been determined; however, there are strong indications that the primary problem area is the CSN-1A t-c set. A more detailed discussion of the problem is presented in Section VI.

The reactor power level (and, consequently, the temperature of the gas delivered to the turbine) was limited by a high indicated temperature on one fuel element. This temperature limit, coupled with the t-c set performance deficiency, limited the self-run capability of the plant at one-half speed to ambient temperatures below 60°F. At higher ambient temperatures full-speed operation of the start motor was required to attain self-run.

The test was terminated by a power failure scram which resulted in the complete loss of electrical power to the major equipment. (This scram was caused by the failure of the insulation in one of the main cables between the control cab and the plant.) As a consequence, the t-c set coasted to a stop in 75 seconds. Fuel element temperatures were maintained below 1100°F by use of the emergency coolant gas system and moderator water flow was not interrupted. However, it was not possible to provide sufficient emergency coolant flow to adequately cool the turbine bearing and the temperature was observed to peak at 404°F. Post-test inspection of the bearing revealed only a slight discoloration due to hot oil deposits.

III. SIGNIFICANT PLANT MODIFICATIONS

During the period between the initial full power and limited endurance tests (ANSOP 16625) and the tests reported herein (ANSOP 16625A), significant modifications were made to the ML-1 plant and test facility in the following areas:

- Reactor and reactor skid
- Reactivity shim liners
- Fuel element spiders
- Fuel element instrumentation
- CSN-1 t-c set
- Flow measuring instrumentation
- Test facility exhaust system

These changes are discussed in detail below.

A. REACTOR REPAIR PROGRAM

Shortly after the shutdown terminating a 100-hr run under ANSOP 16625 in late February and early March 1963, evidence of a process gas leak to the moderator system was observed in the region of the lower plenum of the reactor pressure vessel. Several tests and a review of the design indicated that the leak was probably due to the failure of a seal weld on a shear pin in the lower plenum cap joint. A circular section was cut from the bottom of the shield tank and the reactor pressure vessel was removed through this opening. The lower shield cone was removed and the reactor outlet duct was cut. The upper moderator seal weld (between the inner radial shield assembly and the upper plenum) was also cut prior to removal of the pressure vessel. Although the seal weld of only one shear pin was cracked (attributed to a defective weld), all of the shear pins were repaired by counter-boring and installing seal weld cups (Figure 1).

Several modifications were made to the reactor skid to permit the subsequent removal of the pressure vessel (if required) by disassembling bolted connections rather than cutting welded joints. These modifications included:

FIGURE 1. MODIFIED ML-1 SHEAR PIN DESIGN

- A removable dished head was provided to replace the bottom section of the shield tank removed during disassembly. A flanged joint was installed in the gas outlet duct inside the shield tank.
- The inside of the inner radial shield was enlarged 0.100 in. to permit the installation of the pressure vessel through the top of the shield assembly.
- The welded joints at the upper and lower moderator seals were replaced with bolted, flange-type closures.

In addition, the recess in the exterior of the lower plenum cap was fitted with an aluminum plug to eliminate the air trap at this location and thus reduce the lower plenum cap temperatures during reactor operation.

The major changes accomplished during the reactor repair and re-assembly program are apparent by comparison of Figure 2a and 2b (see pages 8 and 9). A detailed discussion of the reactor repair program was published (Ref. b).

B. REACTIVITY SHIM LINERS

During the reactor defueling in April 1963, it was noted that the reactivity shims were difficult to remove from the pressure tubes. Subsequent examination of the shims revealed some corrosion, primarily on the silver plating on the inside surface, but the "sticking" was determined to be due to excessive bowing. A silver loss of 4.4 to 9.9%, determined by destructive examination of irradiated and new reactivity shims, was attributed to abrasion during insertion, removal and handling of the shims and fuel elements (Ref. 4). The corrosion observed was attributed to galvanic action between the stainless steel liners and thin, porous plating of the silver.

A new set of shims was fabricated and was installed before ANSOP 16625A (Ref. c). These shims were smaller in circumference (5.27/5.28 in. compared to 5.400/5.500 in.), and the combination of this change and more restrictive fabrication tolerances reduced the bowing to a maximum of 0.040 in. (compared to an average of 0.055 in.). The silver plating was applied to the inside surface only. These changes were made to reduce electrolytic corrosion and abrasion of the silver, and to minimize sticking. Three types of shims were installed: unplated (stainless steel) shims, "light" shims (23.7 gm of silver), and "heavy" shims (67.8 gm of silver). The core layout during ANSOP 16625A, including the reactivity shim pattern, is shown in Figure 3.

C. FUEL ELEMENT SPIDER MODIFICATION

Excessive corrosion of all 61 fuel element lower spiders and failure of five of the spiders was observed during an inspection in September, 1963. A program to evaluate the cause of the corrosion and failures and to design and install improved spiders was undertaken from mid-October 1963 to mid-January 1964 (Ref. 3). The failure of the lower spiders was determined to be the result of the use of sub-standard material in the spider castings and unfavorable tolerance buildups during manufacture. The corrosion was subsequently attributed to the use of non-specification AISI Type 316 stainless steel in the spider

FIGURE 2a. ML-1 REACTOR SKID CROSS SECTION BEFORE MODIFICATION

FIGURE 2b. MODIFIED ML-1 REACTOR SKID CROSS SECTION

11.2-64-2223

castings. The chromium content of the material was found to be less than that required to maintain a passive film. The major consequence of the corrosion was the binding of the fuel pin tips in the lower spider. The failures resulted from downward deformation (caused by seizure of the fuel pin tips in the guide holes) and rupture of some of the spider bosses where the wall thickness was reduced by off-center boring of the guide holes. A decision was made to modify all 61 fuel elements even though only five had failed.

A modified lower spider was designed which consists of two machined subassemblies (Figure 4) welded into a spider assembly. This assembly is, in turn, welded in an orifice assembly. All parts of the lower spider/orifice assembly are fabricated from Hastelloy X. The clearance between the fuel pin tips and the guide holes in the lower spider is increased to minimize binding. The modified spider assembly does not engage the center, unfueled pin. Flow tests indicated that the pressure drop across the new spider is the same as the original design although the orifice coefficient is somewhat different. The original and modified configurations of the lower end of the fuel element are shown in Figure 5.

D. FUEL ELEMENT INSTRUMENTATION

During the reactor repair and reassembly program, it was necessary to cut and remove the thermocouples installed for sensing fuel element temperatures. Seven spare ML-1 fuel elements were subsequently modified to accept thermocouples in the unfueled center pins. Immediately following reassembly of the reactor, the seven instrumented fuel elements were installed in the reactor (Figure 3) and the new thermocouples were routed from a junction box in the reactor tank through the pressure vessel upper cap Grayloc seal ring to the fuel elements. The modified design permits remote removal and replacement of the thermocouples thus preventing the destruction of the thermocouples during handling of instrumented fuel elements.

E. CSN-1 TURBINE-COMPRESSOR SET MODIFICATIONS

The CSN-1 t-c set was disassembled, decontaminated and inspected at the NRTS in April 1963. The inspection revealed excessive bearing wear (0.003 to 0.004 in.) and evidence of rubbing of internal rotating parts. The t-c set was returned to Clark Bros., Olean, New York, to permit the installation and testing of improved bearings and installation of new turbine stator nozzle assemblies. A detailed inspection at Clark Bros. revealed cracking of some of the turbine rotor blades at the root of the outer fir-tree serration. This cracking was attributed to an error in the original stress calculations and the turbine rotor blades were redesigned. The new turbine blades were fabricated from higher performance material and the root length was increased 30%.

The improved bearings incorporate a Rayleigh step to force the oil film, by a self-pumping action, to load the shaft more heavily and thus increase the shaft running stability. Four Bently shaft displacement probes for measuring shaft vibration were installed at Clark Bros. and assisted significantly in the testing and evaluation of the new bearings. The set was reassembled with new turbine rotors, with modified (to conform to the CSN-2 design which permits adjustment of the blade angle) turbine stator assemblies, and with the improved bearings. The unit was then tested in the open-cycle configuration to demonstrate the acceptability of the modifications. A total of 92 hr of testing

FIGURE 4. NEW 18-HOLE LOWER SPIDER TACK-WELDED TO NOSE PIECE

FIGURE 5. ML-1-I FUEL ELEMENT MODIFICATION

indicated that the set would perform satisfactorily. In particular, the performance of the new bearing was excellent; vibration amplitude (peak to peak) was less than 0.00035 in. at operating speed (Refs. 5 and d).

The modified t-c set, designated as the CSN-1A because of the aerodynamic modifications, was shipped to the NRTS in February 1964.

F. FLOW MEASURING INSTRUMENTATION

The original design of the ML-1 power plant did not provide a device for measuring the process gas flow. It was planned that mass flow rates could be estimated from the CSN-1 compressor map which was based on the results of open-cycle tests and testing of the power conversion skid where flow sensing devices were installed. Since accurate flow measurements are essential in analyzing power plant performance, a venturi was designed for installation between the reactor outlet and turbine inlet (the only almost-straight ducting run in the compact plant).

A standard Herschel-type venturi was designed in accordance with the ASME Code for Flow Measurements. The venturi section was installed in a new turbine inlet duct assembly as part of the inner liner of the internal duct insulation (Figure 6). Following proof tests of the final assembly, the unit was calibrated at the Aerojet-General Corporation Hydraulic Flow Laboratory in Sacramento. The flow tests were conducted with mocked-up upstream and downstream piping configurations, including the reactor outlet double elbow, and, all plant instrumentation which could affect the venturi readings were simulated during the tests. Reduction of the calibration data resulted in a venturi coefficient of 0.977 and the following flow rate equation:

$$\overset{0}{W} = 8.05 F_a Y_a \sqrt{\frac{P \Delta h_w}{T}}$$

where:

- $\overset{0}{W}$ = weight flow of nitrogen, lb/sec
- F_a = thermal expansion factor
- Y_a = adiabatic expansion factor
- Δh_w = differential head, inches of water at 68°F
- P = inlet pressure, psia

The calibrated venturi-turbine inlet duct assembly was installed in the power plant prior to ANSOP 16625A. The estimated accuracy of the venturi is discussed in Section V of this report.

G. TEST FACILITY EXHAUST SYSTEM

During ML-1 testing prior to ANSOP 16625A, it was noted that hot precooler exhaust air recirculated in the ML-1 test building and mixed with precooler intake air, thereby degrading the precooler and plant performance. An exhaust system was installed in the test building which prevents the air recirculation by ducting the precooler outlet air through the north side of the test building. The system consists of a large hood, mounted directly over the precooler, connected by a 6-ft diameter duct to a 90,000 cfm blower.

FIGURE 6. ML-1 VENTURI-TURBINE INLET DUCT ASSEMBLY

IV. CONDUCT OF EXPERIMENT

A. PREPARATION FOR TEST

Preparations for this test, initiated at the completion of ANSOP 16625 in March 1963, included personnel training and modifications to the power plant and facility.

A comprehensive refresher course in power plant operation was conducted for all operations (both AGN and attached Army) personnel. Approximately 1300 man-hr were expended in this refresher course, and an additional 400 man-hr were expended in specialized maintenance and safety training. Prior to the initiation of the test, six operators and five shift supervisors had passed the written and oral qualification examinations.

Several deficiencies were noted during and subsequent to the performance of ANSOP 16625. More than 100 equipment and facility modifications were implemented to correct these deficiencies prior to the performance of ANSOP 16625A. The major modifications performed during this period are described in Section III.

Final preparations, which included low-power reactor operation, t-c set rotation with cold gas and operation of all sub-systems, were completed within two weeks of the initiation of the test. On 10 April, all power plant auxiliary systems (except for neutron monitoring instrumentation) were shut down. Thus the startup initiated on 13 April was a complete plant startup in every sense.

B. TEST OPERATIONS

The "ML-1 Full Power and Limited Endurance Test (ANSOP 16625A)" was initiated on 13 April 1964. All auxiliary systems and power plant startup procedures were satisfactorily performed with little difficulty. Reactor criticality was attained on 14 April and full t-c set rotational speed was achieved on 15 April after some difficulty in achieving self-sustaining conditions. The power plant operational history during ANSOP 16625A is shown in Figure 7 and self-run times and plant shutdown are tabulated, respectively, in Tables 1 and 2.

TABLE 1 - TURBINE-COMPRESSOR SELF-RUN OPERATIONS

<u>Time</u>	<u>Date</u>	<u>To</u>	<u>Date</u>	<u>Time</u>	<u>Duration</u>	<u>Accum. Time</u>
2318	4/15		4/16	0427	5:09	5:09
0608	4/17		4/17	2016	14:08*	19:17
0134	4/18		4/24	0920	151:55*	171:12
2028	4/27		4/28	1004	13:36*	184:48
1204	4/28		4/28	1903	6:59	191:47
2115	5/4		5/9	1202	110:47	302:34
2347	5/9		5/20	0512	245:25	547:59
0044	5/25		5/29	2118	116:34	664:33

*Self-run at one-half speed

TABLE 2 - PLANT OPERATIONS - SCRAMS

<u>Time</u>	<u>Date</u>	<u>Function</u>	<u>Explanation</u>
0521	4/16/64	Manual	Repair oil line
0249	4/17/64	Moderator	Sensor line frozen
0929	4/24/64	Overspeed	Instrument malfunction
1004	4/28/64	High power	Operator error, spurious signal Channels 5 and 7
1903	4/28/64	Overspeed	Instrumentation malfunction
1202	5/9/64	Moderator low flow	Instrumentation failure
0910	5/20/64	Manual	Repair diesel emergency power source
2118	5/29/64	Power failure	Plant control circuit mal- function

FIGURE 7. ANSOP 16625A POWER PLANT OPERATIONAL HISTORY

11.2-64-2181

FIGURE 8. HOT STOP PARAMETERS FOR SCRAM ON 29 MAY 1964

11.2-64-2182

The highest reactor power level reached during the experiment, obtained on 17 April, was estimated at that time to be the full design reactor power level of 3.3 Mw. However, subsequent calculations, reflecting additional performance information and a thorough analysis of the data, showed that the power level was actually only about 3.03 Mw.

Following the near-rated operation of the plant on 17 April, the reactor power was reduced and the remainder of the test was devoted to completing the endurance objectives of the experiment. In addition to the scrams (listed in Table 2) which resulted in plant shutdowns, a moderator-to-shield water leak was discovered following a shutdown on 24 April and a higher than normal break-away torque was measured in the rotating machinery following the shutdown on 28 April. The moderator-to-shield water leak was found to be of minor consequence and the high break-away torque was eliminated by cycling the t-c set static seals. These problems are discussed in Section VIII.

The test was terminated on 29 May 1964 after 1493.35 Mw-hr of reactor operation and a total of 823.28 hr of t-c set rotation, including 664.55 hr at self-sustaining conditions. The test was terminated by a power failure scram (caused by failure of the insulation on one of the main cables) which interrupted the electrical power to the control system of several major plant electrical components. This power failure scram resulted in a "hot-stop" in which the turbo-machinery coasted to a stop in about 75 seconds. Selected parameters as a function of time following the scram are shown in Figure 8. During this period, the fuel elements were cooled by manual, intermittent injections of emergency cooling gas and the maximum recorded fuel element temperature did not exceed 1100°F. The turbine bearing temperature reached a maximum of 404°F because it was not possible to maintain emergency seal gas flow to cool the bearing. However, the bearing was subsequently found to be unaffected by the heat and the surface appeared to be in excellent condition (see Section VIII-C).

All major test objectives were accomplished during the test and the improvements and correction of mechanical deficiencies implemented prior to the test were satisfactorily demonstrated. In addition, the following instrumentation and control systems developed under the ML-1 improvements program demonstrated excellent performance:

- The integrated process pressure chassis
- The integrated process temperature chassis
- The bearing temperature chassis
- The reactor outlet temperature chassis
- The site area monitor (SAM) system

V. INSTRUMENTATION

A. ANALYSIS INSTRUMENTATION

The instrumentation discussed in this section is primarily that required to provide data for thermodynamic analysis and includes instrumentation for monitoring all process gas pressures, temperatures and flow rates, as well as the instrumentation to provide data on which to base computations of moderator water and lubricating oil heat balances (Figure 9). Detailed information on the instrumentation is tabulated in Appendix B.

1. Temperature Measurement

Temperatures in the ML-1 plant are measured almost exclusively by thermocouples. Grounded junction, sheathed, chromel-alumel thermocouples are used in nearly all cases. The thermocouple output voltages are measured on Bristol multipoint recorders or on Houston voltmeters. The multipoint recorders and the Houston voltmeters are both motor-driven, null balancing, potentiometer-type instruments so that cable resistance errors are eliminated. Pace reference junctions (nominally $150^{\circ}\text{F} \pm 0.2^{\circ}\text{F}$) are used with these systems. The temperature recorders and the Houston voltmeters are checked periodically, using a certified L & N potentiometer. A typical thermocouple system is shown in the sketch on page 25. The calibration routinely performed before and after each test minimizes errors in the data although small inaccuracies may result from reading errors, reference junction temperature error, and residual recorder errors.

2. Pressure Measurement

Barton mechanical pressure gauges, observed by a closed-circuit television system, are used for all analysis pressure measurements. Reading errors, gauge temperature coefficient errors, and gauge hysteresis errors are not eliminated by the normal calibration "add-error" curves. The reading errors should be less than one-half of the smallest dial marking or $\pm 0.5\%$ of full scale. The remaining error (hysteresis and wear) should be less than the manufacturer's guarantee (i.e., $\pm 0.5\%$ of full scale). Errors in the Barton gauge due to cold weather operation can be significant for all of the gauges except the moderator water core pressure differential gauge. The estimate of probable error for various Barton gauges is included in Appendix B.

NOTE: NUMBERS IN PARENTHESIS ARE EXPLAINED IN APPENDIX B.
NUMBERS IN TRIANGLES ARE STATE POINTS.

FIGURE 9. ANALYSIS INSTRUMENTATION SENSOR LOCATIONS ML-1

3. Flow Rate Measurement

The analysis flow measuring instrumentation consists of either an orifice or a venturi installed in the standard configuration. The data generated by these devices are read out on Barton differential pressure and absolute pressure gauges. The compressor seventh stage seal gas flow rate, which is of secondary importance for analytical purposes, is measured with a balanced strain gauge bridge. Measured flow rates and estimated errors due to the readout system are tabulated in Appendix B.

a. Process Gas Flow

The turbine inlet venturi was calibrated against a master venturi at the AGC Hydraulic Flow Laboratory in Sacramento on 24 January 1964. The master venturi had previously been calibrated by use of a calibration tank at flows below 200 lb/sec and with a precision Potter flowmeter at flows above 200 lb/sec. The maximum error in the flow coefficient (and the resulting calibration curves) was estimated to be less than $\pm 1.4\%$.

b. Moderator Water Flow

The moderator water flow rate is obtained from a correlation which relates flow rate to pressure drop across the reactor core. The correlation used was determined experimentally by measuring flow rates with a standard orifice plate flow meter and corresponding reactor core pressure drops. The error in this correlation was estimated to be less than $\pm 1.5\%$ over the moderator temperature range of 70° to 180°F . The Barton differential pressure gauge used in this system has excellent temperature compensation ($\pm 0.4\%$ over 100°F). The maximum flow rate error, including Barton gauge error and gauge reading error, is less than 2.3% .

c. Bypass Gas Flow

The bypass gas flow is determined by a venturi and Barton gauge. The differential pressure indicated by the gauge is referred to a table which relates flow rates to differential pressure readings and ambient pressures. These data were computed for a mean gas temperature of 227°F using a standard venturi coefficient. The maximum error decreases with bypass flow rate and is estimated to be less than $\pm 11\%$ at low flow rates.

d. Seventh Stage Compressor Bleed (Seal Gas Flow)

The calibration tables for this venturi/Barton gauge system were calculated as described above for the bypass gas flow. The flow rate and temperature of this system is relatively constant and the maximum error is estimated to be less than $\pm 10\%$. Most of the estimated error is due to the relatively poor stability and accuracy of the strain gauge pressure transducer which serves as the sensor for the system.

4. Power Measurements

a. Shaft Output Power

The output power of the plant is calculated from the torque and speed as determined respectively by the dynamometer and digital revolutions counter. The accuracy of the dynamometer system is $\pm 0.5\%$ of the indicated torque value. The dynamometer torque indicating system is calibrated periodically with standard weights. The error in the digital revolutions counter system is considered to be negligible. This system is periodically checked at five intervals of speed over the full range with a precision square wave generator.

b. Start Motor Power

The electrical power wattmeter which indicates the start motor power requirement has a guaranteed accuracy of $\pm 1\%$ of full scale (-100 to + 100 kw) or ± 2 kw.

B. VIBRATION INSTRUMENTATION

1. Bently Probes

Four Bently probes (distance measuring transducers) were installed in the CSN-1A and used during open-cycle tests at Clark Bros. during December 1963 and January 1964. The probes measure the vertical and horizontal displacement of the t-c set shaft relative to the turbine and compressor radial bearings. The output of the Bently probes was generally monitored on a panoramic analyzer which displayed the vibration amplitudes over a large frequency spectrum.

2. Linear Vibration Measurement

Linear vibration sensors were installed on the CSN-1A t-c set and on the start motor and dynamometer. These sensors are self-generating, velocity type transducers. The transducers yielded good results although the low frequency response was found to be somewhat limited during calibration prior to the test. The output signals from these probes were monitored on an instrument which indicated vibrational amplitudes directly.

3. Torsional Vibration Measurement

Torsional vibration of the rotating machinery was measured by a "Torsiograph"* mounted on the outboard end of the dynamometer shaft. The self-generated output signal from the Torsiograph was monitored on a continuous strip chart recorder with adjustable chart speed.

C. NUCLEAR INSTRUMENTATION

Instruments are provided to monitor the neutron flux from source level to full reactor power. Duplicate channels are provided in each of three over-lapping ranges (Table 3).

Provision is made to calibrate the startup channels (1 and 2) at four counting rates, the intermediate channels (3 and 4) at four flux levels, and the high-level channels (5, 6 and 7) at the zero and 100% power points. Three period-calibration signals are provided to check this function.

TABLE 3 - ML-1 NUCLEAR INSTRUMENTATION

<u>Channels</u>	<u>Detector Type**</u>	<u>Range</u>	<u>Readout</u>	<u>Scram</u>
1 and 2	B ¹⁰ lined proportional counters	8.3×10^{-2} to 8.3×10^4 nv	Log count rate	Period scram
3 and 4	Gamma compensated ionization chambers	10^3 to 3.5×10^9 nv	Log N power	Period scram
5, 6 & 7	Uncompensated ionization chambers	4.5×10^7 to 4.5×10^9 nv	Log N power	High level scram

* Trade name of Consolidated Electrodynamics Corp.

**The detectors are located in dry, polyethylene-lined chambers mounted in the reactor shield tank.

VI. AEROTHERMODYNAMIC PERFORMANCE

A. SUMMARY

The maximum plant output power of 139 kw (185 kw total thermodynamic power output) and the maximum reactor power of 3030 kw (2533 kw to gas) were recorded on 17 April 1964. These maxima were not attained at the same data point; the maximum power output occurred at 90 psia compressor inlet pressure, and the maximum reactor power at 120 psia. The summary of pertinent parameters of the maximum power points is given in Table 4.

Plant output was restricted by a high indicated temperature on a single fuel element which limited the reactor outlet temperature to approximately 1150°F (design value is 1200°F). By way of comparison, the maximum power output during ANSOP 16625 occurred on 4 March 1963 at 1200°F reactor outlet temperature and 16°F ambient, when 252 kw as produced at the dynamometer shaft. Had these conditions been duplicated in ANSOP 16625A, extrapolation by computer indicates the power output would have been about 250 kw. This value is considerably less than that predicted from an analysis of the turbine modification discussed on page 11, et seq. The compressor performance did not improve as had been expected and was characterized by a shift in the speed characteristic resulting in a reduction of approximately 7% in flow and only a slight rise in pressure ratio. It is suspected that partial stall of one or more compressor stages was primarily responsible for this condition.

The evaluations and conclusions discussed in this section are based on data generated during the conduct of ANSOP 16625A. The raw data were reduced by a special computer code (DRML-1*) which permitted the treatment of a large volume of data in a consistent manner.

The results of the data reduction were correlated to determine component characteristics (pressure drop and effectiveness curves, machinery maps, etc.). The complete set of component characteristics were then input to a digital systems analysis code (CHOP - See Ref. e) and problems were run to develop an overall correlation to relate plant output to environmental conditions. The measured plant outputs and those calculated by the CHOP code were in good agreement, which lends credence to the entire analysis.

The state point subscript notation used throughout this section refers to the state point definitions shown in Figure 9.

*DRML-1 is an unpublished code written at AGN.

TABLE 4 - SUMMARY OF TEST DATA
(ANSOP 16625A)

Maximum Reactor Power

Reactor power to gas	2633 kw
Turbine inlet temperature	1147°F
Turbine speed	22,000 rpm
Compressor inlet pressure	120 psia
Compressor inlet temperature	105°F
Precooler air inlet temperature	58°F
Shaft power to dynamometer	76 kw

Maximum Output Power

Reactor power to gas	2267 kw
Turbine inlet temperature	1149°F
Turbine speed	22,000 rpm
Compressor inlet pressure	90 psia
Compressor inlet temperature	77°F
Precooler air inlet temperature	50°F
Shaft power to dynamometer	139 kw

Endurance Operating Conditions

Reactor power to gas	1900 to 2100 kw
Turbine inlet temperature	1025° to 1150°F
Turbine speed	22,000 rpm
Compressor inlet pressure	85 to 90 psia
Compressor inlet temperature	55° to 115°F
Precooler air inlet temperature	40° to 95°F
Shaft power to dynamometer	22 to 100 kw

B. DATA REDUCTION

A fundamental requirement for the evaluation of system and component performance is the formulation of a systematic method of data reduction. The primary function of the method is the determination of consistent values for the system state points to permit the calculation of the system performance parameters.

An IBM 7094 computer code (DRML-1*) was written as a basic tool for the reduction of data. This code determines the most probable values of the primary loop state point temperatures from experimental measurements. The code also calculates the system performance parameters from these determined temperatures and other prepared input data.

1. Data Preparation

The calculations required before the data was input to the code are listed below. In addition, the experimental data was corrected for instrument calibration errors (as shown in Appendix B) before being used in the code.

- 1) Pressures: The state point pressures were determined by adjusting the measured pressures (P_i) and the pressure drops (ΔP) so that the system pressures balanced around the primary loop. In adjusting the pressures, care was taken to ensure minimal changes in the measured values. Two factors were used as guides when establishing changes:
 - a) the measured pressure drops require less change than the measured pressures because of the greater accuracy of pressure drop measurements, and,
 - b) the high pressure loop pressure drops are consistent functions of the coolant flow.
- 2) Flows: The turbine flow was calculated from the data developed by the turbine inlet venturi, and the compressor flow was determined by summing the turbine flow and the bypass and seal gas flows.
- 3) Thermodynamic Power: The thermodynamic power was calculated by summing the measured dynamometer power with the t-c set parasitic losses. Parasitic power was defined as the loss in shaft power due to frictional drag and was determined by the measurement of the total energy deposition in the lubricating oil passing through the turbine, compressor, and alternator bearings, the L-seal, the gearbox and the thrust bearings (see Figure 10).
- 4) Precooler Inlet Air Temperature: The precooler inlet air temperature was determined by averaging the measured temperatures in the precooler fan plenums.
- 5) Ducting Temperature Drops: The ducting temperature drops were estimated as a function of process gas flow around the loop (Appendix E).

*DRML-1 is an unpublished code written at AGN.

FIGURE 10. CLARK CSN-1A TURBINE COMPRESSOR SET PARASITIC POWER LOSS VERSUS SHAFT SPEED

6) Precooler Air Flow: The precooler volumetric air flow was assumed to be constant at 778 ft³/sec. This value was derived from reduction of data obtained earlier (Ref. 6).

7) T-C Set Speed: The t-c set speed was calculated directly from measurements.

8) Recuperator shell Loss: The recuperator shell loss was assumed to be 0.5 Btu/lb.

2. DRML-1 Code Format

The fundamental function of DRML-1 is to determine the most probable values of six basic state point temperatures ($T_1, T_2, T_4, T_7, T_8, T_{10}$ - see Figure 9) in the primary loop from the experimentally measured values. The basic method used is the statistical principle of maximum likelihood (see Appendix C). This method adjusts the measured temperatures to values that satisfy the t-c set and recuperator energy balances and the laws of statistical probability.

The code calculation procedures and the pertinent equations, are as follows:

1) The statistically averaged values of the six temperatures are calculated from the groups of measurements at each of the six state points.

2) The enthalpy values (H_i) for the corresponding temperatures ($\bar{T}_i$) are interpolated from tables in the code which were extracted from a Franklin Institute publication (Ref. f). Energy balances for the t-c set and recuperator are made using these enthalpy values and the corresponding enthalpy variances by the following expressions:

$$(H_8 - H_{10}) - (H_4 - H_2) = A \quad (\text{Energy balance for the recuperator}) \quad (1)$$

$$W_t (H_7 - H_8) - W_c (H_2 - H_1) = B/1.055 \quad (\text{Energy balance for the t-c set}) \quad (2)$$

where:

W_c = compressor flow, lb/sec

W_t = turbine flow, lb/sec

A = recuperator shell loss, Btu/lb

B = plant thermodynamic power, kw

H_i = enthalpy of gas at state point i, Btu/lb

The enthalpy values are adjusted, based on the statistical principle of maximum likelihood, to satisfy the energy balance equations 1 and 2. The corresponding temperatures are determined from these state point enthalpies and the remaining six state point temperatures and enthalpies are calculated by means of pre-determined duct temperature losses between thermocouple stations. Thus, the most probable temperatures and enthalpies at all state points are determined.

3) The relative pressure method (Appendix D) based on a pressure of 225 psia is used to determine the isentropic state point temperatures and enthalpies (H_1') for the compressor and turbine efficiency calculations.

4) The inputs for thermodynamic power, compressor flow, turbine flow, precooler inlet air temperature, t-c set speed and state point pressure are provided and the following parameters are calculated:

Reactor power to gas - energy addition to the process gas by the reactor; calculated by:

$$kw = 1.055 W_t (H_6 - H_5)$$

Recuperator shell loss - an input quantity multiplied by the flow rate as given by the equation:

$$kw = 1.055 W_t (A)$$

Compressor efficiency (η_c) - ratio of isentropic compressor work to actual compressor work as given by the equation:

$$\eta_c = \frac{H_2' - H_1}{H_2 - H_1}$$

Turbine efficiency (ϵ_t) - ratio of actual turbine work to isentropic turbine work as given by the equation:

$$\epsilon_t = \frac{H_7 - H_8}{H_7 - H_8'}$$

Recuperator effectiveness (ϵ_r) - defined by the equation:

$$\epsilon_r = \frac{T_4 - T_3}{T_9 - T_3}$$

Precooler effectiveness (ϵ_p) - defined by the equation:

$$\epsilon_p = \frac{T_{11} - T_{12}}{T_{11} - T_a}$$

Precooler $C_{\min}/C_{\max}$ - ratio of the heat capacities of the coolant gas to precooler air flow as given by the equation:

$$C_{\min}/C_{\max} = \frac{W_c \frac{H_{11} - H_{12}}{T_{11} - T_{12}}}{(\rho C_{pa}) (PCVW)}$$

where:

ρC_{pa} = product of ambient air density and specific heat (table of this product vs temperature is incorporated into the code), Btu/ft³-°F

PCVW = precooler volumetric ambient air flow, ft³/sec

The code prints out the statistically averaged state point temperatures and enthalpies, the hand-balanced pressures, and the resulting performance parameters. In addition, the code prints out the most probable temperature and performance values in parentheses. A sample print-out appears in Appendix F.

3. Data Points Evaluated

About 100 of the 393 data points taken during ANSOP 16625A were reduced to provide information for performance evaluation. A selected group of data points are included in Appendix F.

Using the component characteristic data calculated from the balanced or "most probable" state point variables, as explained above, the functional characteristics of each major system component were determined by correlating those data points which represented the greatest variety of conditions.

The resulting correlations are discussed in the following sections. Wherever possible, component performance is compared to design values or to the performance observed during ANSOP 16625.

C. TURBINE-COMPRESSOR SET PERFORMANCE

The results of the evaluation of t-c set performance data obtained during ANSOP 16625A were to be compared with the results from earlier tests (ANSOP 16625 - Ref. a). An apparent shift in the CSN-1A compressor speed characteristic lines during ANSOP 16625A cast some doubt on the validity of the flow data obtained during ANSOP 16625. The flow rate W_1 was obtained during ANSOP 16625 by entering the published compressor map for the CSN-1 with values of pressure ratio, P_2/P_1 , and referred speed, $N/\sqrt{T_1}$, and determining the resulting flow function, $\frac{W_1 \sqrt{T_1}}{P}$. This method assumes that the published compressor map is correct and therefore any shift in compressor speed characteristics is undetectable.

The question arises as to whether or not the observed speed characteristic shift resulted from the t-c set modification prior to ANSOP 16625A or was present and undetected in the CSN-1 unit (during ANSOP 16625). Re-examination of open- and closed-cycle test data produced inconclusive results, primarily because of the doubtful validity of the flow data obtained during these tests. If, however, it is assumed for the purposes of analysis that the speed characteristic shift was present earlier (ANSOP 16625) and reflects the difference between operation under open- and closed-cycle conditions, the actual compressor and turbine flow rates would be about 7% lower than those indicated by the published compressor map. This would have a relatively small effect on compressor and turbine efficiencies, but the turbine flow function would also be reduced about 7%.

In the region of the design point, the change in turbine flow function at a given turbine expansion ratio is approximately proportional to the change in turbine effective flow area. Thus, if the measured flow function curve from the current tests (ANSOP 16625A) is compared with the earlier curve

(ANSOP 16625) reduced by 7%, the results indicate that the effective flow area was reduced only 6% despite of the measured 13% reduction in flow area. In addition, a comparison of open-cycle test results before and after the turbine modification indicates a 13% reduction in the turbine flow function, thus confirming the effect of the area reduction.

If the above assumption is correct and the flow calculations for the earlier tests (ANSOP 16625) were 7% high, then a direct comparison of the two closed-cycle tests should indicate a 20% decrease in the turbine flow function. This observation would be correct even if the method of converting open-cycle air data to closed-cycle nitrogen data was not empirically correct because the method is consistently applied in each case. Since a 20% reduction in the turbine flow function was not observed, it is reasonable to conclude that the CSN-1 and CSN-1A compressor maps are not the same and that the shift in the speed characteristic did occur coincidentally with the turbine modification. It can also be concluded that in all probability the flow calculations for ANSOP 16625 were approximately correct and that the CSN-1 compressor map was valid for those tests.

The open-cycle compressor map data generated at Clark Bros. (Ref. 7) exhibited a shift in the speed characteristic on one test; however, subsequent cleaning of the compressor restored the speed lines to the original position. Examination of the compressor blading following ANSOP 16625A revealed considerable buildup of foreign material but to a lesser degree and of a different character than that observed at Clark Bros. In addition, boundary layer flow patterns were observed in this material which revealed that radial flow existed in the first stage compressor rotor over the outer 30% of the blades. This observation lends credence to the hypothesis that at least the first stage rotor was partially stalled, probably disturbing the flow patterns throughout the entire compressor. Further development of this hypothesis, however, is not possible in the absence of considerable compressor interstage data.

The performance of the CSN-1A compressor during ANSOP 16625A is presented on the compressor map (Figure 11) and the work parameter and efficiency plot in Figure 12. Figures 13 and 14 are cross-plots of the compressor map parameters showing the effect of the apparent speed characteristics shift.

The CSN-1A compressor operating line indicates a displacement toward the surge line precisely as predicted; however, an apparent shift to a lower flow function of the speed characteristic lines results in only a slight increase in pressure ratio at a given referred speed, $N/\sqrt{T_1}$. The compressor work parameter, $\Delta h/T_1$, and compressor efficiency are presented as a function of compressor pressure ratio in Figure 12 for both CSN-1A and CSN-1. A comparison of the actual and predicted values of pressure ratio as a function of $N/\sqrt{T_1}$ is shown in Figure 13. As can be seen, the data of ANSOP 16625A correlates reasonably well with ANSOP 16625, indicating that the compressor efficiency did not improve with the turbine modification as had been expected. This result may be related to the suspected flow distortion in the CSN-1A compressor.

Detailed examination of the data indicated that changes occurred in the turbine flow function during the test. Prior to the shutdown at Data Point 35, the turbine flow followed the circles on Figure 15; however, after startup the flow decreased abruptly as shown by the x's on Figure 15. Following the

FIGURE 11. CLARK CSN-1 AND CSN-1A COMPRESSOR CHARACTERISTICS
(CLOSED CYCLE NITROGEN)

FIGURE 12. CLARK CSN-1A COMPRESSOR PERFORMANCE
(ANSOP 16625A)

FIGURE 13. CLARK CSN-1A COMPRESSOR PRESSURE RATIO VERSUS REFERRED SPEED^{*}
(ANSOP 16625A)

FIGURE 14. ML-1 CLARK CSN-1A COMPRESSOR REFERRED FLOW
 VERSUS REFERRED SPEED (ANSOP 16625A)

FIGURE 15. CLARK CSN-1 AND CSN-1A TURBINE FLOW FUNCTIONS

shutdown at Data Point 115, the flow increased slightly, and the data exhibited good correlation for the remainder of the test. In addition, significant scatter and poor correlation with the CHOP output is exhibited for the data prior to Point 115, but very good correlation exists with the data after Point 115, lending some support to the apparent flow function shift. Although the results are inconclusive, the indicated shift could be related to the movement of the turbine nozzle shim ring discovered during the CSN-1A disassembly inspection subsequent to the test.

The turbine work parameter, $\Delta h/T_7$, is presented as a function of turbine expansion ratio in Figure 16 for the CSN-1 and CSN-1A. As was the case with the compressor, no significant changes due to the turbine area modification are evident. Turbine and compressor efficiencies, which have historically exhibited a large degree of scatter, have been correlated by drawing smooth curves through the actual and ideal work parameter points and then ratioing the curves to obtain the efficiencies.

To correlate the compressor and turbine work parameters and thereby determine the common operating point, it is necessary to establish the relationship of the compressor pressure ratio to the turbine expansion ratio and present this ratio in terms of one of the remaining compressor map parameters. Since it is desirable to establish these relationships independent of the flow rate, the compressor referred speed, $N/\sqrt{T_1}$, was selected as the third parameter. Figures 13 and 17 present the compressor pressure ratio and turbine expansion ratio as functions of compressor referred speed, $N/\sqrt{T_1}$. With these data it is possible, knowing the compressor inlet temperature and rotor speed, to determine the compressor and turbine work parameters and efficiencies for a given configuration. Unfortunately, only limited data were obtained at other than the design rotor speed (22,000 rpm) and therefore the slope of the curves has been verified only within a small range of pressure ratios experienced at design speed conditions. It is expected that the full range will be thoroughly investigated during subsequent plant testing. As an extension of the work parameter curves, Figures 18 and 19 present the compressor and turbine work, Δh , and t-c set net work as a function of compressor and turbine inlet temperatures. It is obvious that the effect of a compressor inlet temperature change at 22,000 rpm (Figure 18) is significantly greater on turbine work than on compressor work.

The following conclusions are based on the analysis of the CSN-1A performance:

- 1) A shift in the CSN-1A compressor speed characteristic curves occurred between open- and closed-cycle testing, resulting in a reduction in compressor pressure ratio (of 8.6%) and flow function (of 7%) at design referred speed from that predicted from the published compressor map. The reason for this shift is not completely clear; however, there are indications, based on the analysis of open-cycle test data (Ref. 7), and the post-test inspection of the compressor blading, that one or more of the compressor stages may be operating in partial stall, causing an effective change in compressor flow geometry. In addition, the methods for converting compressor open-cycle air data to closed-cycle nitrogen data, while theoretically valid, have not been empirically verified. Further investigations in this area are scheduled.

FIGURE 16. CSN-1A TURBINE PERFORMANCE

FIGURE 17. CSN-1A TURBINE EXPANSION RATIO VERSUS COMPRESSOR REFERRED SPEED

112-64-2191

FIGURE 18. CSN-1A COMPRESSOR AND TURBINE WORK VERSUS COMPRESSOR INLET TEMPERATURE AT DESIGN ROTOR SPEED (ANSOP 16625A)

FIGURE 19. CLARK CSN-1A TURBINE COMPRESSOR SET NET WORK VERSUS COMPRESSOR AND TURBINE INLET TEMPERATURES, ANSOP 16625A

2) The lack of significant improvement in the CSN-1A output can be directly attributed to the low turbine and compressor efficiencies. It was expected that the turbine flow area modification would improve the match between the turbine and compressor, and permit the compressor to operate in a more efficient region although no significant increase in turbine efficiency was expected. The CSN-1A turbine and compressor efficiencies are essentially the same as those of the CSN-1 (Figure 12 and 16). The reason for the lack of improvement in compressor efficiency could be related to the shift in compressor speed characteristics and is certainly explained by the partial stall hypothesis.

3) The assumption that the ANSOP 16625 flow calculations were 7% high, indicating that the compressor speed characteristic shift had occurred prior to that test, is not supported by analysis. If the assumption was correct, then the turbine flow parameter during ANSOP 16625A would have indicated a 20% decrease in effective flow area when compared to the ANSOP 16625 data (Ref. a). The open-cycle test before and after the turbine modification indicated a 13% decrease in effective flow area using a consistent flow measuring system. It is therefore reasonable to expect the same 13% decrease to be indicated during closed-cycle testing. Since the expected 13% reduction in the turbine flow parameter, based on published data (Ref. a, 8, 9), is observed (when using the compressor map for ANSOP 16625 and turbine venturi measurements for ANSOP 16625A), then it must be concluded that the flow measurements obtained for each of the tests are consistent and therefore that the CSN-1 compressor map was essentially valid for the ANSOP 16625 tests.

4) The changes in the turbine flow function presented in Figure 15 indicate that a slight shift in performance may have occurred.

D. REACTOR PERFORMANCE

1. Reactor Power

The reactor thermal power to gas was computed by the DRML-1 data reduction code from the enthalpy rise across the reactor core and the mass flow rate determined from the turbine inlet venturi. The enthalpy rise across the core was derived from temperatures determined by the DRML-1 which accounts for heat losses from the gas ducts and thermocouple measuring errors due to radiant heat transfer to the cooler duct walls (see page 31, et seq.). An analysis of the errors introduced due to the duct heat losses and thermocouple radiant losses is presented in Appendix E.

The total reactor thermal power was obtained by performing an energy balance on the reactor. The moderator water heat load and other estimated thermal losses from the reactor were added to the reactor thermal power to gas. Since the moderator water heat load calculation is based on a small temperature differential (less than 10°F) and a high flow rate (approximately 300 gpm), a small error in temperature measurement results in a large error in computed moderator heat load (1°F is approximately equivalent to 47 kw). Consequently, considerable uncertainty exists in the computations of moderator water heat balance. In an attempt to improve the accuracy of the determination of the energy dissipated in the moderator, two sets of thermocouples were

installed prior to ANSOP 16625A and connected to indicate the differential temperature between the moderator inlet and outlet water. Although the data obtained from these systems contained considerable scatter, more consistent moderator heat loads were calculated than with the previous data. The moderator heat load was calculated from the temperature differential data at more than forty data points. Comparison of these values with reactor power-to-gas values at the same data points resulted in a correlation which indicated that the moderator heat load is approximately 13% of the reactor power to gas. An analysis of design operating conditions predicts that the heat deposition in the moderator water should be about 9% of the reactor power to gas. For the analysis discussed in this report, the 13% value is used.

In addition to moderator heat load, approximately 2% of the reactor power to gas is estimated to be lost to the shield water from the gas ducts and nuclear heating of the metal shielding and shield water. Thus, the total reactor power is estimated by increasing reactor power to gas values by 15%.

The maximum reactor power reached during these tests (ANSOP 16625A) was 3.03 Mw (2.63 Mw to gas) at Data Point 30. The computed reactor thermal power to gas is presented in Figure 20 as a function of the recorded nuclear power on Channel 3 Log N power recorder for power levels ranging from 0.45 to 2.63 Mw. This plot exhibits correlation within $\pm 15\%$ of the mean.

2. Hot Spot Temperatures

The ML-1-I reactor core loading included seven instrumented fuel elements during ANSOP 16625A (Figure 3). A thermocouple was installed in the unfueled center pin at an X/L of 1.0 in five of these elements and at X/L of 0.4 in the remaining two. The instrumented fuel element temperatures data were analyzed to estimate the maximum hot spot temperature experienced in the core during the experiment and to explain the higher than expected temperature reading from the fuel element in core position 5-F.

Twenty random data points between data points 17 and 225 were used for this analysis. To determine the degree of consistency of the data from the seven fuel element thermocouples, the following ratio was computed from the data:

$$\frac{T_{T/C} - T_{in}}{T_o - T_{in}} \quad (3)$$

where:

- $T_{T/C}$ is the temperature indicated by the fuel element thermocouple
- T_{in} is the reactor coolant inlet temperature
- T_o is the reactor coolant outlet temperature

The ratio for the seven instrumented fuel elements was tabulated (Table 5). The ratio as calculated varies within approximately $\pm 10\%$ of the mean value, but if one of the extreme datum points of each fuel element is neglected, the data spread decreases to approximately $\pm 7\%$ of the mean value.

FIGURE 20. COMPUTED REACTOR POWER-TO-GAS VERSUS NUCLEAR POWER RECORDED ON CHANNEL 3 LOG N POWER RECORDER (ANSOP 16625A)

TABLE 5 - INSTRUMENTED FUEL ELEMENT TEMPERATURE RATIOS

$$(T_{T/C} - T_{in}) / (T_o - T_{in}) \text{ FOR ANSOP 16625A*}$$

Data Point	Fuel Element						
	5-F	2-H	8-E	1-A	7-G	5-E	3-C
17	1.47	1.195	1.21	1.035	1.11	0.639	0.649
18	1.467	1.158	1.17	1.01	1.108	0.624	0.66
19	1.48	1.21	1.20	1.032	1.11	0.639	0.66
20	1.48	1.21	1.19	1.025	1.07	0.582	0.655
25	1.31	1.03	1.215	0.93	0.99	0.585	0.59
26	1.56	1.27	1.31	1.07	1.16	0.665	0.665
27	1.52	1.225	1.24	1.05	1.135	0.645	----
28	1.51	1.20	1.10	1.01	1.10	0.633	0.643
30	1.50	1.175	1.165	0.99	1.072	0.61	0.625
49	1.505	1.26	1.255	1.00	1.12	0.633	0.633
51	1.55	1.25	1.275	1.02	1.125	0.651	0.633
52	1.535	1.26	1.285	1.00	1.133	0.64	0.63
67	1.55	1.305	1.33	1.06	1.16	0.665	0.653
80	1.55	1.28	1.30	1.025	1.14	0.65	0.65
88	----	1.28	1.28	0.985	1.14	0.628	0.638
111	1.53	1.279	1.29	0.99	1.135	0.648	0.641
129	1.53	1.29	1.32	1.045	1.145	0.64	0.652
148	1.525	1.275	1.265	1.025	1.15	0.648	0.651
164	1.57	1.29	1.33	1.025	1.168	0.671	0.652
225	1.57	1.272	1.37	1.05	1.16	0.663	0.662

*Reactor core layout is illustrated in Figure 3.

A temperature measurement error of approximately $\pm 20^\circ\text{F}$ in the fuel element and reactor outlet thermocouples would be sufficient to cause this data spread. This degree of consistency was considered satisfactory for the analysis in question.

The reactor core hot spot temperatures were computed from the reactor core inlet and outlet temperature data. A method reported earlier (Ref. 10) was used to perform these calculations. The results for the selected data points show that the computed hot spot temperatures were all well below the 1750°F limit (Table 6).

The hot spot temperature of the fuel element in core position 5-F was also computed. The temperature reading predicted in fuel element 5-F was computed as follows:

$$T_{(5-F)_{\text{predicted}}} = T_{\text{in}} + 1.347 (T_o - T_{\text{in}})_{\text{meas.}} \quad (4)$$

The hot spot temperature was computed by the following expression:

$$\frac{(T_{\text{HS}(5-F)} - T_{\text{in}})}{(T_{\text{HS}(\text{core})} - T_{\text{in}})} = \frac{(T_{(5-F)_{\text{meas.}}} - T_{\text{in}})}{(T_{(5-F)_{\text{predicted}}} - T_{\text{in}})} \quad (5)$$

The results of these calculations indicate that the hot spot temperature of the fuel element in core position 5-F probably did not exceed the 1750°F limit (Table 7).

Two sets of HECTIC problems (Ref. g and h) were run to compute the expected fuel element thermocouple reading from the fuel element power and flow rate. The results of these computer problems are compared with the data from Data Points 30 and 57 in Table 8. The individual fuel element hot spot temperatures were computed in the same manner as in the previous paragraph. The hot spot temperatures of the fuel element in core position 5-F computed by the two methods are in good agreement (Tables 7 and 8).

The data in Tables 7 and 8 show that all fuel elements except the one in core position 5-F, operated near to the predicted temperatures. In particular, both the overall power and flow profile in the core appear to be as predicted. This is verified, in part, by the fact that the thermocouple in the fuel element in the central (5-E) core position reads very close to the predicted value. The general conclusion drawn is that the high reading observed from the fuel element in core position 5-F is unique to this fuel element. The possible causes for the high temperature could be:

- Improper orifice size
- Flow blockage in the fuel element
- Instrument error
- Measurement of hot spot effects by the thermocouple
- Measurement of effects not included in the calculational methods

The first two possible causes have been eliminated by the post-shutdown inspection of the fuel element. This inspection revealed that the element had the specified orifice and that the coolant flow passages in the element were clear of flow obstructions.

Analysis indicates that an instrumentation error of approximately +60°F is required to account for the observed temperature reading. Since it is not likely that the instrumentation error could be this large, this possible cause was eliminated.

The thermocouple could have sensed some of the effects included in the hot spot analysis (Ref. 11), specifically the fuel element power effect ($\pm 5\%$) or the fuel element flow rate effect ($\pm 3\%$). It is not considered likely that the thermocouple was sensing the effect of other hot spot factors

TABLE 6 - CALCULATED REACTOR CORE HOT SPOT TEMPERATURES*

	D A T A								P O I N T								
	17	18	19	20	26	28	30	49	57	52	67	80	88	111	129	148	164
W (meas)	15.75	16.35	16.4	16.4	12.65	18.5	23.2	17.6	17.6	17.6	17.3	17.66	17.6	17.6	18.0	19.3	16.0
T _o (meas)	1090	1090	1075	1059	1090	1150	1149	1125	1120	1015	1085	1132	1047	1068	1090	1100	1105
Correction	13	12	12	12	16	12	9	11	12	10	11	12	11	11	11	10	12
T _o (actual)	1103	1099	1087	1071	1108	1162	1158	1137	1132	1025	1096	1144	1058	1079	1101	1110	1117
T _{in} (meas)	697	692	677	677	707	718	745	702	709	630	680	732	653	664	695	701	700
T _b (core)	406	407	410	404	401	444	413	435	423	395	406	412	405	145	406	409	417
NTU	1.82	1.81	1.81	1.81	1.91	1.77	1.69	1.80	1.80	1.80	1.8	1.8	1.80	1.80	1.78	1.76	1.83
ε	0.737	0.735	0.735	0.735	0.757	0.725	0.706	0.732	0.732	0.732	0.732	0.732	0.732	0.732	0.728	0.723	0.74
1.47 $\frac{T_b(\text{core})}{T_{HS}(\text{core})}$	810	815	821	810	779	900	860	874	850	794	815	827	814	834	820	832	829
T _{HS} (core)	1507	1057	1498	1487	1486	1618	1605	1576	1559	1424	1498	1559	1467	1498	1515	1533	1529

*NOTATION FOR TABLES 6 and 7

W - Coolant flow rate, lb/hr

T_o - Gas outlet temperature, °FT_{in} - Gas inlet temperatureT_b(core) - Bulk temperature rise across the core, °F

NTU - Number of exchanger heat transfer units

ε - Emissivity, dimensionless

T_{5-F} - Temperature of fuel element in core position 5-F, °FT_{HS}(core) - Temperature, hot spot, °F

TABLE 7 - FUEL ELEMENT 5-F HOT SPOT TEMPERATURES*

	D A T A P O I N T																
	17	18	19	20	26	28	30	49	57	52	67	80	88	111	129	148	164
1.347 T _b (core)	547	549	552	545	540	598	556	586	570	532	546	555	546	559	546	551	562
T _{5-F} (calc)	1244	1241	1229	1212	1247	1316	1301	1288	1279	1162	1246	1288	1199	1223	1241	1252	1262
T _{5-F} (meas)	1294	1290	1284	1266	1334	1391	1366	1357	1365	1236	1315	1371	----	1300	1315	1325	1355
(T _{5-F} - T _{in})meas	597	598	607	599	627	673	621	655	656	606	635	639	----	636	620	624	655
(T _{5-F} - T _{in})meas																	
(T _{5-F} - T _{in})calc	1.09	1.09	1.10	1.10	1.16	1.125	1.118	1.118	1.15	1.14	1.162	1.15	----	1.164	1.137	1.32	1.165
T _{HS} (core) - T _{in}	810	815	821	820	779	900	860	874	850	794	818	827	----	834	820	832	829
(T _{HS} (core) - T _{in}) (T _{5-F} - T _{in})meas (T _{5-F} - T _{in})calc	883	888	904	903	904	1013	963	977	977	905	950	951	----	965	933	943	965
T _{HS} (5-F)	1580	1580	1581	1570	1611	1731	1708	1679	1686	1535	1630	1683	----	1629	1628	1644	1665

TABLE 8 - ANSOP 16625A FUEL ELEMENT HOT SPOT TEMPERATURESFOR DATA POINTS 30 AND 57HECTIC RESULTS

	Data Point 30						
	Fuel Element						
	5-F	2-H	8-E	1-A	7-G	5-E	3-C
x/L of thermocouple	1.0	1.0	1.0	1.0	1.0	0.4	0.4
T _{measured}	1366	1231	1227	1153	1198	996	1003
T _{HECTIC}	1305	1242	1242	1171	1185	1021	1028
T _{o element}	1212	1189	1189	1122	1126	1176	1204
T _{meas} - T _{in}	621	486	482	408	453	251	258
T _{HECTIC} - T _{in}	560	497	497	436	440	276	273
$\left(\frac{T_{\text{meas}} - T_{\text{in}}}{T_{\text{HECTIC}} - T_{\text{in}}} \right)$	1.11	0.978	0.97	0.937	1.03	0.91	0.946
Fuel Element Hot Spot							
$860 \times \left(\frac{T_{\text{meas}} - T_{\text{in}}}{T_{\text{HECTIC}} - T_{\text{in}}} \right)$	964	840	834	805	885	782	813
T _{HS} (Fuel Element)	1709	1585	1579	1550	1630	1527	1548

	Data Point 57						
	Fuel Element						
	5-F	2-H	8-E	1-A	7-G	5-E	3-C
x/L of thermocouple	1.0	1.0	1.0	1.0	1.0	0.4	0.4
T _{measured}	1365	1241	1248	1142	1186	985	977
T _{HECTIC}	1281	1221	1221	1154	1164	995	1002
T _{o element}	1184	1163	1163	1000	1101	1148	1176
T _{meas} - T _{in}	656	532	539	433	477	276	268
T _{HECTIC} - T _{in}	572	512	512	445	455	286	293
$\left(\frac{T_{\text{meas}} - T_{\text{in}}}{T_{\text{HECTIC}} - T_{\text{in}}} \right)$	1.15	1.04	1.05	0.975	1.047	0.965	0.917
Fuel Element Hot Spot							
$850 \left(\frac{T_{\text{meas}} - T_{\text{in}}}{T_{\text{HECTIC}} - T_{\text{in}}} \right)$	977	884	892	828	890	820	779
T _{HS} (Fuel Element)	1686	1593	1601	1537	1609	1529	1588

(such as uncertainty in control rod effect on power, in the pin-to-pin power ratio and in the degree of coolant mixing) since these factors would be expected to influence the temperature of the adjacent instrumented fuel element by the same amount. Thus, assuming that the actual fuel element power was on the high side of the tolerance and the flow rate was on the low side, it is possible that the thermocouple in the fuel element in core position 5-F was measuring part of the hot spot effects. This could account for approximately 35°F of the 60°F difference observed.

3. Reactor Pressure Drop

The reactor pressure drop parameter, $P\Delta P$, is plotted as a function of coolant flow rate in Figure 21. The pressure drop parameter is the product of the core inlet pressure and the core pressure drop. The data scatter from this experiment is much less than that experienced during ANSOP 16625 and 16615. At full power (Data Point 30), the measured reactor pressure drop was 14 psid at a measured process gas flow rate of 23 lb/sec.

The ANSOP 16625A experimental data indicated that the reactor pressure drop has increased by approximately 15% since ANSOP 16625, based on the assumption that the calculation of coolant flow rate during both experiments was correct. This pressure drop increase can be attributed to two factors.

- 1) The reactor core had been re-orificed between the two tests.
- 2) The method used to determine process gas flow rate differed in the two tests.

The re-orificing analysis (Ref. 12) indicated that the core pressure drop would increase by approximately about 7% or 1 psid.

The ANSOP 16625A reactor pressure drop was approximately 10% less than that computed for the ML-1-I reference state point. The ANSOP 16625 and 16615 pressure drops were about 25% less than the reference value. This difference of about 8% in the deviation from the reference value can be attributed to the different methods used to determine the coolant flow rates between the two experiments and to the relatively large scatter of data.

E. RECUPERATOR

Figure 22 indicates that the recuperator effectiveness is about 70%, and relatively independent of flow rate. When the data from ANSOP 16625 are normalized by the energy balance technique used in this report, the value for recuperator effectiveness is also about 70%. The difference between this and the design value (78%) amounts to less than 10 kw in net output power, but has an appreciable effect on the precoolers inlet temperature and fuel element hot spot temperature.

The recuperator high pressure side pressure drop is plotted as a function of pressure and flow in Figure 23. The indicated pressure drop on the low pressure (shell) side includes a large instrument error, but appears to average about 0.5 psid at 18 lb/sec nitrogen flow rate and 90 psia.

FIGURE 21. REACTOR PRESSURE DROP PARAMETER $P\Delta P$ AS A FUNCTION OF COOLANT FLOW RATE DURING ANSOP 16625 AND 16625A

FIGURE 22. RECUPERATOR EFFECTIVENESS VERSUS TURBINE FLOW (ANSOP 16625A)

FIGURE 23. PRESSURE LOSS IN HIGH PRESSURE RECUPERATOR SIDE
(State Points 2 to 5 - ANSOP 16625A)

F. PRECOOLER

It was found that the installation of the precooler air exhaust duct notably improved the performance of the precooler. A direct comparison of temperature rise from ambient to precooler fan outlet temperature has not been made because only two fan outlet thermocouples were installed during ANSOP 16625, whereas six were installed during ANSOP 16625A (Figure 9). The readings of the six varied so widely that an average of only two would be misleading. However, an approximate comparison can be made by plotting compressor inlet temperature minus ambient temperature against nitrogen flow. Ambient temperature was defined as the average of the temperatures sensed by two thermocouples located outside of the fan plenums. As the flow approaches zero, these curves should be asymptotic to the temperature rise to the fan outlets. Figure 24 shows that this temperature rise has been reduced by about 20°F; this amounts to a gain of 40 to 50 kw in plant output.

The precooler effectiveness is plotted against compressor flow in Figure 25. Such a plot is valid only for constant ambient temperature, so the effectiveness for each point was corrected for the difference between the precooler fan outlet temperature and 60°F. This was done by first plotting the uncorrected points, from which the effect of the flow stream capacity weight ratio (C_{min}/C_{max}) on effectiveness was measured. Then, for each point, the effect of ambient temperature on C_{max} was calculated (assuming constant volumetric air flow), the effectiveness was corrected, and the points replotted on Figure 25.

The design effectiveness is 92% at 25.3 lb/sec or about 4% higher than that shown in Figure 25. In the precooler air flow test (Ref. 6) prior to ANSOP 16625A, it was found that the core volumetric air flow is considerably less than the design value. This condition appears to be responsible for a significant portion of the low precooler effectiveness.

Nitrogen pressure drop through the precooler averaged about 2 psid at 18 lb/sec and 90 psia inlet pressure. The data scatter was too large to permit meaningful conclusions from the precooler pressure drop data.

G. OVERALL POWER PLANT PERFORMANCE

Analysis of the plant output led to three conclusions:

- The output can be plotted as a function of only compressor inlet pressure, P_1 , and the ratio of turbine inlet temperature to compressor inlet temperature, T_7/T_1 , thus making it possible to directly display data, predict output, and accurately calculate influence coefficients with one graph (Figure 26).
- The output values plotted on Figure 26 are consistent with the values calculated from the component characteristic correlations (discussed in the preceding sections) by the CHOP code. This result confirms the validity of the method used to correlate the data.
- For every combination of ambient and turbine inlet temperature, there is an optimum compressor inlet pressure for maximum output power.

FIGURE 24. COMPARISON OF TEMPERATURE RISE FROM AMBIENT TO COMPRESSOR INLET

FIGURE 25. PRECOOLER EFFECTIVENESS VERSUS COMPRESSOR FLOW
(ANSOP 16625A Points Corrected for a Precooler Air Temperature of 60°F)

FIGURE 26. PARAMETRIC PERFORMANCE DIAGRAM

Throughout this section, "ambient temperature" is defined as the average of the six precooler fan plenum thermocouples, and "thermodynamic output" is the dynamometer shaft power plus parasitic losses.

1. Parametric Performance Diagram

The component functional characteristics, as discussed in the preceding sections, were inputted into the CHOP systems analysis code (Ref. e), and the plant output was calculated for assumed environmental conditions. The calculated plant output is plotted as solid lines on Figure 26. Since the measured output (the X's on Figure 26) is in good agreement with the calculated values, the methods of data reduction and correlation used above are considered reliable.

Up to the time of this analysis, no satisfactory method had been developed to display the output of the ML-1 without the use of estimated influence coefficients, nor had any method been developed to specify influence coefficients without an almost insuperable number of qualifications. The reason for this is that the full-speed output is always a function of three variables: turbine inlet temperature (T_7), compressor inlet temperature (T_1), and compressor inlet pressure (P_1), or three dependents of these. Appendix G demonstrates that these three variables can be reduced to two (compressor inlet pressure and the ratio of T_7/T_1), thus making it possible with one graph to directly display data, predict output, and accurately calculate influence coefficients.

Figure 26 describes the full-speed output of the ML-1 during ANSOP 16625A under all conditions. As a corollary, this figure can be used to calculate environmental influence coefficients at any operating condition. If it were known, the compressor surge line could also be plotted directly on the graph. The curves on Figure 26 are accurate only for the system as it existed during ANSOP 16625A. However, both a computer analysis and a theoretical derivation (Appendix G) indicate that this method of plotting output correlations is valid within the accuracy of the experimental data for all Brayton cycles operating at rated speed and above a specified minimum ratio of turbine inlet temperature to compressor inlet temperature, T_7/T_1 .

Influence coefficients can be calculated at any operating condition by using this graph and the equations below:

$$\frac{\partial kw}{\partial P_1} = \text{slope of lines on Figure 26 (Influence of compressor inlet pressure)} \quad (6)$$

$$\frac{\partial kw}{\partial T_7} = \frac{\partial kw}{\partial (T_7/T_1)} \times \frac{1}{T_1} \quad (\text{Influence of turbine inlet temperature}) \quad (7)$$

$$\frac{\partial kw}{\partial T_1} = \frac{kw}{\partial (T_7/T_1)} \times \frac{T_7}{T_1^2} \quad (\text{Influence of compressor inlet temperature}) \quad (8)$$

$$\frac{\partial kw}{\partial T_{amb}} = \frac{\partial kw}{\partial (T_7/T_1)} \times \frac{\partial T_1}{\partial T_{amb}} \times \frac{T_7}{T_1^2} \quad (\text{Influence of ambient temperature}) \quad (9)$$

where:

$\frac{kw}{(T_7/T_1)}$ is spacing of lines on Figure 26 at operating point, and,

$\frac{T_1}{T_{amb}}$ is slope of lines on Figure 27.

2. Effect of System Pressure on Power Output

Figure 28, developed from the experimental data as described below, shows that an optimum compressor inlet pressure exists for each combination of ambient and turbine inlet temperatures. This is to be expected, since as pressure increases, flow increases, and precooler effectiveness decreases. Eventually, plant output must reach a maximum and then decrease because of the progressively less effective precooler. The approximate optimum pressures are estimated from Figure 28 and plotted on Figure 29.

Figure 27 is based on the assumption that the compressor inlet temperature, T_1 , is independent of turbine inlet temperature, T_7 . A computer analysis of component data from this test (Ref. 8, 9) showed that a 200°F change in turbine inlet temperature, T_7 , changes compressor inlet temperature, T_1 , only 1.5°F at 120 psia, and less at lower pressures. This analysis is supported by the good agreement between the solid lines (calculated from component characteristics) and the data points plotted on Figure 27. It is estimated that this assumption is valid if precooler effectiveness is greater than 85%.

Using Figure 27 in conjunction with Figure 26, plant output and compressor inlet temperature can be predicted for any combination of ambient temperature, compressor inlet pressure, and turbine inlet temperature (all independent variables). Figure 28 is constructed by combining Figures 26 and 27 to eliminate compressor inlet temperature, T_1 .

An example of this optimum pressure phenomenon is shown on Figure 30. Figures 26 and 27 were used to calculate the solid line for an ambient of 60°F and turbine inlet of 1100°F. Data points at various pressures were corrected for these temperature, using the above influence coefficient equations, and plotted as the x's on Figure 30. The optimum pressure appears to be about 90 psia.

FIGURE 27. PRECOOLER PERFORMANCE AS A FUNCTION OF PRESSURE(22000 rpm)

FIGURE 28. THERMODYNAMIC OUTPUT POWER AT FULL SPEED

FIGURE 29. APPROXIMATE COMPRESSOR INLET PRESSURE FOR MAXIMUM OUTPUT(22000 rpm)

FIGURE 30. EFFECT OF PRESSURE ON OVERALL PLANT OUTPUT

VII. NUCLEAR PERFORMANCE

A. SHIELDING

In the following discussion, primary emphasis has been placed on presenting data in a form that can be readily assimilated and that permits direct comparison of predicted performance, measured data, and design criteria. It was necessary, in several instances, to make obvious extrapolations (as for example, from actual power to design power) to develop data which could be compared consistently. This was done only in cases where extrapolation was quite straightforward and not subject to significant error. The data have also been corrected for instrument drift and non-linearity. With these qualifications, all data presented are actual measured points.

Not all data recorded during ANSOP 16625A is reported in this section. Data not directly relevant to operation of the ML-1 under field conditions (such as measurements behind artificial obstructions), or which was obviously in error because of experimental difficulties, were omitted. Uncorrected data have not been reported because of the difficulties of presenting such data in readily understandable form.

1. Facility

A schematic diagram of the location of the major features of the AGN-NRTS test site is shown in Figure 31. Stakes were driven into the ground at appropriate intervals along the axes shown in this figure to provide consistent points at which shielding data could be recorded during power operation. A polar coordinate grid was marked on the floor of the test building to define the location of measurements made after shutdown.

The test area was the same as it had been during the conduct of ANSOP's 16615 and 16625 with two major exceptions:

- 1) A sheet metal hood and duct installed over the precooler to minimize recirculation of ambient air in the building during operation
- 2) An extension constructed on the north side of the test building to house the shield water handling equipment

FIGURE 31. ML-1 SITE MAP

112-64-2203

During operation of the plant, the hanger-type doors at each end of the test building were normally left open to ensure adequate air circulation. With these doors open, there is an unobstructed view of the entire ML-1 plant along the 0° , 345° , and 330° axes (Figure 31).

2. Instrumentation

a. Neutron Detection

Fast neutron survey meters* were used for the detection of neutrons. These instruments have a tissue-equivalent response for neutrons in the range of 0.2 to 1.4 Mev and read out in units of mrep/hr. There are three dose rate ranges, 0-5, 0-50 and 0-500 mrep/hr. The probe contains a gas-filled, proportional proton recoil counter. The manufacturer's stated accuracy is $\pm 10\%$ for plutonium-beryllium neutrons.

These instruments are generally satisfactory if calibrated carefully but the slow response time, particularly on the 0-5 scale, is disadvantageous for a hand-carried instrument. They are also subject to considerable drift from the zero point and readings below about 0.5 mrep/hr are not reliable.

b. Gamma Detection

Geiger-Mueller counters** were used for the majority of the gamma surveys. The four dose rate ranges are 0-0.2, 0-2, 0-20 and 0-200 mrep/hr. The stated accuracy is $\pm 2.5\%$ of full scale for the lowest three ranges and $\pm 7.5\%$ for the high range. These instruments are compact and easy to operate but are limited in range.

For the higher gamma dose measurements, a "Radector" survey instrument*** was used. This instrument has two logarithmic scale ranges from 0.5-500 mr/hr and from 0.5-500 r/hr. Stated accuracy is $\pm 15\%$ of indicated dosage anywhere on the scale. The greatest disadvantage of these instruments is the small size of the scale face which makes accurate reading difficult.

c. Calibration of Instruments

All of the instruments were calibrated at the NRTS Central Facility and were checked with gamma and neutron sources at the GCRE facility periodically throughout ANSOP 16625A. Calibration curves for the various instruments used in this experiment are presented in Figures 32-34.

3. Shutdown Shielding Measurements

a. Reactor Operating History

It was necessary to compile a reactor power history to evaluate the decay of radiation after plant shutdown. This was done for the

*Manufactured by Nuclear Corporation of America

**Eberline, Model E-510

***Manufactured by Victoreen

FIGURE 32. CALIBRATION OF FAST NEUTRON SURVEY METER

FIGURE 33. CALIBRATION OF GEIGER - MUELLER COUNTER
 (Manufactured by Eberline)

112-64-2205

FIGURE 34. CALIBRATION OF "RADECTOR" SURVEY INSTRUMENT
(Manufactured by Victoreen)

purpose of shielding analysis and does not represent a detailed history (Figure 35). This history was further simplified as indicated below; these values are considered to adequately represent ANSOP 16625A for shielding analysis.

<u>Run</u>	<u>Power, Mw(t)</u>	<u>Duration, hr</u>	<u>Integrated Power, Mw-hr</u>	<u>Shutdown Date</u>
1	2.2	200	440	4-24-64
2	2.2	400	880	5-20-64
3	2.2	<u>100</u>	<u>220</u>	5-29-64
	TOTAL	700	1540	

The total duration and the accumulated megawatt-hours compare well with those which were compiled from the detailed operational history.

b. Predictions of Shutdown Shield Performance

Evaluation of the shutdown shielding requires a prediction of the anticipated dose rates at specific locations for the conditions of the experiment. Two representative locations were chosen: These were at 25 ft from reactor centerline in the 0° and 195° directions. Both of these points are readily surveyed. The point at 25 ft in the 0° direction with the power conversion skid in place is the condition for which the ML-1 shutdown dose criteria are specified. Measurements along the 195° axis are representative of the "back side" of the reactor and avoid the contaminated demineralizer (located on the 180° axis) which influences local tank surface measurements. Information from The ML-1 Design Report (pp 47-49, Ref. i) was used to predict the expected dose rates at these locations. These curves show the individual activation components which contribute to the total dose rate. In predicting doses following ANSOP 16625A, these curves were corrected for differences in operating power, length of operation, dose measurement location and reactor configuration.

Reference full power for the ML-1 is 3.3 Mw(t), but the reactor was assumed to have operated continuously at 2.2 Mw(t) during these tests. Since the magnitude of the radiation dose is a direct function of the irradiation power level, all dose rates were multiplied by the power ratio (i.e., $R = 0.667$).

The duration of reactor operation affects both the magnitude of the residual dose and the subsequent shape of the decay curve if several radioisotopes with different half-lives are present. For the aluminum and tungsten activations, the half-lives are short enough that activity saturation and simple decay with 15- and 24-hr half-lives, respectively, could be assumed. The stainless steel and fission product activities were not saturated during ANSOP 16625A, however, and it was necessary to prepare curves showing the relative shapes and magnitudes of these activities as functions of reactor operating time. The fission product decay was assumed to follow the Way-Wigner relationship (Figure 36). The stainless steel decay curves, taken from those published in the ML-1 Shielding Design Report (Ref. j), are shown in Figure 37. A cobalt content of 0.08 wt% was assumed. From these curves, composite plots

* NOTE: THIS HISTORY COMPILED FOR SHIELDING EVALUATION
AND DOES NOT REPRESENT A DETAILED HISTORY OF ANSOP 16625A

FIGURE 35. REACTOR POWER HISTORY* (ML-1, ANSOP 16625A, APRIL - MAY, 1964 TOTAL REACTOR THERMAL POWER)

FIGURE 36. WAY-WIGNER RELATIONSHIP FOR GAMMA POWER AFTER REACTOR SHUTDOWN $P/P_O = (3.11 \times 10^{-2}) [T_{SD}^{-0.2} - (T_{OP} + T_{SD})^{-0.2}]$

FIGURE 37. SHUTDOWN DOSE RATE ANTICIPATED FROM ML-1 AS A RESULT OF STAINLESS STEEL ACTIVATION FOR VARIOUS OPERATING TIMES AT 3.3 Mw (t). (DOSE MEASURED AT 25 ft FROM REACTOR CENTERLINE ON 0° AXIS WITHOUT POWER CONVERSION SKID AND WITH TANK EMPTY; 0.08 wt % COBALT; 10 wt % BORIC ACID DURING OPERATION pp 19-21, Ref i).

were made that considered the varying decay times from the three individual runs. These plots were then compared to the 10,000-hr reference operating duration, and the ratio was used to normalize the individual decay curves at a reference shutdown time of 24 hr to yield the prediction for the decay of individual contributors following ANSOP 16625A.

The ratio of doses from the 0° to the 180° axis was found by comparing the individual contributors (Table 9). The individual contributors to the dose rate have a somewhat different importance at the back side of the shield and, because of this, the total decay curves have a significantly different shape.

TABLE 9 - SOURCE AND MAGNITUDE OF SHUTDOWN DOSE
RATE 25 FT FROM CORE CENTERLINE*

Source	Dose Contribution, mrem/hr	
	<u>Cab Direction</u>	<u>Back of Shield</u>
		(1½ inch outer lead region)
Fission product gammas	5.0	32.0
Photoneutrons (moderator drained)	1.7	7.75
Activation of control blade shaft	3.5	7.0
Control blade slots	1.0	4.0
Plenum activation		
(fast neutron)	5.1	13.0
(thermal neutrons)	1.7	4.4
Gas duct activation	6.0	75.0
Tungsten activation	0.8	4.0
Aluminum can		
(inner)	3.0	10.7
(outer)	1.1	3.3
Moderator duct	1.0	1.0
Inner stainless steel can	<u>2.1</u>	<u>6.6</u>
TOTAL	32.0**	168.75

*p. 85, Ref. j

**Effect of the power conversion skid is not included. The power conversion skid will reduce the shutdown dose rate by a factor of 3 in the cab direction.

In the early stages of the ML-1 design, it was assumed that the moderator water could be drained after shutdown. The shutdown photoneutron dose rates were, therefore, calculated in earlier work as if the moderator were absent. It has been found necessary to retain the moderator after shutdown for the removal of afterheat. This greatly increases the effective core multiplication and raises the photoneutron dose. This effect was taken into account in predicting the shutdown dose rate.

c. Comparison of ANSOP 16625A Shutdown Data
with Predictions

Figure 38 compares measured shutdown gamma radiation levels following ANSOP 16625A with those predicted. The measurement point is 25 ft from the reactor centerline along the 195° axis and the shield tank is assumed to be empty. The solid, smooth curves represent the predicted levels with the four major contributors (tungsten, aluminum, stainless steel and fission products) and the predicted total. The experimental data is indicated by the points which have been connected by straight lines.

Data was recorded for 16 days after shutdown at which time the major disassembly of the reactor was begun. During the first 24 hr after shutdown, the shield tank was full of 10 wt% boric acid shielding solution. The data taken during this period were corrected to the empty tank condition. Repair and modification work being performed during the shutdown period (handling of activated materials outside of the core) increased the radioactive background. For this reason, the data which were taken at low dose levels and at relatively long times after shutdown are considered unrealistically high.

During the period from one-half to ten days after shutdown, the shape of the decay curve follows the Way-Wigner curve of fission product decay quite closely and is about 20-40% below prediction, indicating that stainless steel may not contribute as much to the total dose rate as was previously thought. Neither the analytical methods of dose prediction nor the measurement techniques are accurate enough, however, to be sure that such a conclusion is justified. The most reasonable conclusion from these measurements is that the dose rate agrees remarkably well with design calculations.

Similar curves were prepared for a point 25 ft from the reactor core on the 0° axis (Figure 39). These data were computed or measured with the power conversion skid in place. In this case, the data taken up to 10 days after shutdown are in almost perfect agreement with the predicted total although, again, the low magnitude of the first datum might indicate that the stainless steel is not dominant.

There are several difficulties connected with measurements taken at this particular point:

- 1) Low dose levels are unduly influenced by building background.
- 2) The extreme heterogeneity of the power conversion skid as a shield causes wide variations in dose levels measured from point-to-point at the same approximate location. The doses quoted were measured at waist level where power conversion skid attenuation is least and are, therefore, somewhat conservative.
- 3) The present power conversion skid configuration is not the reference design since it does not include the reference alternator and start motor. Inclusion of this equipment would increase the average density of the skid and, consequently, reduce the dose rate.

FIGURE 38. COMPARISON OF GAMMA DOSE RATES MEASURED ALONG 180° AXIS FOLLOWING ANSOP 16625A WITH DESIGN PREDICTIONS FOR EQUIVALENT OPERATING CONDITIONS. (DOSES MEASURED AT 25 ft FROM REACTOR WITH TANK EMPTY.)

FIGURE 39. COMPARISON OF GAMMA DOSE RATES MEASURED ALONG 0° AXIS FOLLOWING ANSOP 16625A WITH DESIGN PREDICTIONS FOR EQUIVALENT OPERATING CONDITIONS. (DOSES MEASURED AT 25 ft FROM REACTOR WITH POWER CONVERSION SKID IN PLACE AND TANK EMPTY.)

FIGURE 40. COMPARISON OF PHOTONEUTRON DOSE RATES MEASURED FOLLOWING ANSOP 16625A WITH DESIGN PREDICTIONS FOR EQUIVALENT OPERATING CONDITIONS. (DOSES MEASURED AT 25 ft FROM REACTOR ALONG INDICATED AXES WITH TANK EMPTY.)

The conclusion to be drawn from these data is that the design predictions are quite accurate.

Photoneutron dose rates were predicted for both locations based upon fission product decay schemes determined by the Way-Wigner equation and normalized to the more detailed calculations summarized in the ML-1 Shielding Design Report, (pp. 28-40, Ref. j). Figure 40 shows the predicted decay curves and the measured data. The neutron survey meters used are calibrated in mrep/hr. In all cases, a Relative Biological Effectiveness (RBE) factor of 10 was used to convert mrep/hr to mrem/hr.

The dose rate measured on the 195° axis was approximately one-fourth that predicted during ML-1 design. The decay curve followed closely the curve predicted by the Way-Wigner equation. At 25 ft on the 0° axis, the neutron dose rates were below the level at which the survey instruments would give reliable readings. This is consistent with the readings taken behind the reactor. From these measurements, it is concluded that the full-power, full-term photoneutron dose rates from the ML-1 will be about 25% of those predicted previously.

Figures 41 and 42 compare total dose rates (neutrons and gammas) measured at the two locations with those predicted during the design analysis. The measured dose rates are approximately 40-50% of those predicted for these locations. Figure 42 also shows the criterion to which the ML-1 system was designed. This point is not directly comparable to the curve since the criterion was established for 10,000 hr of full power operation. It does illustrate, however, how near to the fully activated state the system was after ANSOP 16625A.

d. Isodose Maps

Isodose maps for shutdown dose rates 24 hr after shutdown from ANSOP 16625A are shown in Figures 43-45. These figures show gamma, neutron, and total dose rates, respectively, when the shield tank was drained. Figure 46 shows a similar plot for the full-tank situation. In the latter case, the photoneutron dose rate has been reduced to a negligible value by the shield solution so that the gamma dose rate is effectively the total dose.

4. Operational Shielding Measurements

a. Reactor Configuration

During the course of ANSOP 16625A, the ML-1 was operated both with and without the wood expedient shield over the reactor, but always with a 10 wt% solution of boric acid in the shield tank. The large, hanger-type doors at each end of the ML-1 test building were left open for almost all of the shielding measurements.

b. Predictions of Operational Shield Performance

The characteristics of the operational shield were not as thoroughly investigated during the ML-1 design as were those of the shutdown shielding. This was primarily because much less emphasis was placed on the

FIGURE 41. COMPARISON OF TOTAL DOSE RATES MEASURED ALONG 195° AXIS FOLLOWING ANSOP 16625A WITH DESIGN PREDICTIONS FOR EQUIVALENT OPERATING CONDITIONS. (DOSES MEASURED AT 25 ft FROM REACTOR WITH TANK EMPTY.)

11.2-64-2213

FIGURE 42. COMPARISON OF TOTAL DOSE RATES MEASURED ALONG 0° AXIS FOLLOWING ANSOP 16625A WITH DESIGN PREDICTIONS FOR EQUIVALENT OPERATING CONDITIONS. (DOSES MEASURED AT 25 ft FROM REACTOR CENTERLINE WITH POWER CONVERSION SKID IN PLACE AND TANK EMPTY.)

FIGURE 43. SHUTDOWN GAMMA DOSE RATES IN mrem/hr, 24 hr AFTER SHUTDOWN FROM ANSOP 16625A, SHIELD TANK EMPTY

11.2-64-2215

FIGURE 44. SHUTDOWN NEUTRON DOSE RATES IN mrem/hr, 24 hr AFTER SHUTDOWN FROM ANSOP 16625A, SHIELD TANK EMPTY

11.2-G4-2216

FIGURE 45. SHUTDOWN TOTAL DOSE RATES IN mrem/hr, 24 hr AFTER SHUTDOWN FROM ANSOP 16625A, SHIELD TANK EMPTY

11.2-64-2217

FIGURE 46. SHUTDOWN GAMMA DOSE RATES IN mrem/hr, 24 hr AFTER SHUTDOWN FROM ANSOP 16625A, SHIELD TANK FULL - 10 wt % BORIC ACID

11.2-64-221B

operational performance of the shield since it was felt that any inadequacies could be easily remedied by the use of expedient shielding in the field. For this reason, the design predictions of the operational shield performance were based on simplified models and approaches, and were only expected to be accurate to within a factor of three. These predictions are included in the following discussion for comparison with the measured data.

c. Comparison of ANSOP 16625A Operational Data
with Predictions

The majority of operational dose measurements during ANSOP 16625A were taken along the 0° , 345° , and 330° axes. In these directions there is an almost unobstructed view of the ML-1 plant so that the dose rates are very nearly representative of those that would exist if the reactor were operated on flat, open terrain. In the other directions, building walls, auxiliary equipment and various terrain features interfere with the "view" of the reactor and dose rates are correspondingly less.

Figure 47 shows gamma dose measurements taken along the 0° axis out to 500 ft from the reactor centerline, both with and without the wood shield. The slight difference between the two sets of data is considered to be within the experimental error. The design prediction for the dose rate along the 0° axis is also shown on this figure. The measured dose rate is approximately 50% of the predicted dose rate. A theoretical curve for the attenuation of dose rate with distance in air from a point source is also shown. This curve is normalized to the dose rate recorded at 100 ft. The purpose of this curve is to evaluate the importance of air-scattering of gamma radiation to the dose rate along this axis.

Air-scattered radiation tends to attenuate with a reciprocal dependence with distance (i.e., $1/r$ where r = radius) from the source while radiation transmitted from an isotropic source has a reciprocal squared ($1/r^2$) characteristic. Since the slope of the $1/r$ curve is not as steep as that of the $1/r^2$ function, a dose measurement plot that has a significant air-scattered component will be flatter than the theoretical curve shown in Figure 47. The deviation of the measured data from the theoretical curve in this figure is not large and is strongly dependent on the distance for which the curve is normalized. Although there is some indication that air-scattering is a factor, the fact that the presence of the wood shield has no significant effect on dose rate tends to discount this. It is concluded, therefore, that air-scattering of axial gamma radiation from the ML-1 without expedient shielding is not a significant dose contributor, at least when the plant is in the test building.

Similar measurements were made of the neutron dose rates along the 0° axis with and without the wood expedient shield (Figure 48). The design predictions for this location and theoretical normalizations to point isotropic sources are also shown in Figure 48. In the case of neutrons, the wood shield has a pronounced effect. This is to be expected since the wood is about six times more effective in shielding neutrons as it is for gamma radiation and the axial leakage of neutrons from the ML-1 reactor is known to be significant.

Measurements made with the wood shield in place agree well with the theoretical curve for the isotropic point source out to about 350 ft. At that distance, dose levels fall below the level at which the instrument gives reliable readings and the remainder of the curve was extrapolated, as indicated by the "X" data points. The shape of this curve indicates that air-scattered neutron radiation has been effectively eliminated by the wood shield.

When the wood shield was removed, the total dose rate was increased by a factor of 2 to 3. The shape of the curve also began to deviate markedly from that for the isotropic point source. This indicates that air scattering of axially leaking neutrons is a significant contributor to the dose rate without the wood shield. Both curves are well below the design prediction.

Although the neutron dose rates are significantly increased when operating without the wood shield, the resulting effect is not large enough to cause problems in ML-1 operation or to materially increase the health hazard at the NRTS.

The measured total dose rate recorded along the 0° axis during ANSOP 16625A is shown in Figure 49. The measured points are those for the case without expedient shielding (i.e., ML-1 as-built). The dose rates are approximately 50% of those predicted during ML-1 design. The dose rate criterion which was to be satisfied by ML-1 is also shown. This criterion was to be met, if necessary, by the use of field-expedient shielding. The measurements indicate the requirement for such shielding.

d. Isodose Maps

Isodose maps of the gamma, neutron and total dose rate fields are shown in Figures 50-52. These maps were made by combining measurements made along the 0° , 345° , and 330° axis during ANSOP 16615 with data from ANSOP 16625A. Measurements were made to a maximum of 500 ft; the data beyond this point were extrapolated, where necessary, by assuming the reactor to be a point isotropic source and considering air attenuation. This is sufficiently accurate for practical purposes.

The dose rate contours shown were drawn as if the power plant were operated at full power, without expedient shielding on flat, open terrain. Implicit in the curves is the assumption that the presence of the test building has not seriously distorted the data taken along the 0° axis. While this is believed to be true, a limited analytical effort is being prepared to test the validity of this assumption.

The actual dose rate contours around the ML-1 in its existing facility have not been prepared since they would be extremely complex and are very strongly influenced by unique terrain, building and equipment characteristics. The dose rates are, of course, always less than those indicated in Figures 50 to 52.

FIGURE 47. COMPARISON OF GAMMA DOSE RATES MEASURED DURING ANSOP 16625A WITH DESIGN PREDICTIONS

FIGURE 48. COMPARISON OF NEUTRON DOSE RATES MEASURED DURING ANSOP 16625A WITH DESIGN PREDICTIONS

FIGURE 49. COMPARISON OF TOTAL DOSE RATES MEASURED DURING ANSOP 16625A WITH DESIGN PREDICTIONS

GAMMA DOSE RATES IN mrem/hr AROUND ML-1 OPERATING AT 3.3 mw (t) ON FLAT, OPEN TERRAIN. 10 wt % BORIC ACID IN SHIELD TANK - WITHOUT EXPEDIENT SHIELDING.(EXTRAPOLATED FROM ANSOP'S 16615 AND 16625A)

FIGURE 50. ISODOSE PLOT OF GAMMAS, BASED ON ANSOP'S 16615 AND 16625A

11.2-64-2222

GAMMA DOSE RATES IN mrem/hr AROUND ML-1 OPERATING AT 3.3 mw (t) ON FLAT, OPEN TERRAIN. 10 wt % BORIC ACID IN SHIELD TANK - WITHOUT EXPEDIENT SHIELDING.(EXTRAPOLATED FROM ANSOP'S 16615 AND 16625A)

FIGURE 51. ISODOSE PLOT OF FAST NEUTRONS BASED ON ANSOP'S 16615 AND 16625A

11.2-64-2224

GAMMA DOSE RATES IN mrem/hr AROUND ML-1 OPERATING AT 3.3 mw (t) ON FLAT, OPEN TERRAIN. 10 wt % BORIC ACID IN SHIELD TANK - WITHOUT EXPEDIENT SHIELDING.(EXTRAPOLATED FROM ANSOP'S 16615 AND 16625A)

FIGURE 52. ISODOSE PLOT OF TOTAL DOSE RATE, BASED ON ANSOP'S 16615 AND 16625A

11.2-6A-2225

B. REACTIVITY

One of the basic and most difficult problems in the design of a nuclear reactor is the prediction of the nuclear behavior of the reactor during the operating lifetime. The reactivity changes expected during the ML-1 reactor operating lifetime had been calculated and the data obtained during the significant operating time accumulated under ANSOP 16625A permitted a preliminary evaluation of these calculations.

1. Data

The ANSOP 16625A data used in the reactivity analyses included:

- Reactor coolant inlet and outlet temperature
- Reactor coolant inlet and outlet pressure
- Reactor moderator inlet and outlet temperature
- Control rod positions
- Reactor power as indicated by Channels 5, 6, and 7
- Clock time

Each of these items was recorded at about two-hour intervals during the power runs. The resulting 395 data points were all used in the reactivity analysis.

2. Data Reduction

The initial excess reactivity of the ML-1 system in a given reference cold condition (at ambient pressure and temperature) is known to be 1.728% $\delta k/k$. At any time throughout the lifetime of the core, the sum of the excess reactivity, plus the changes associated with non-reference conditions, plus the changes due to fuel burnup must equal this value. Expressed in mathematical terms;

$$k = k_{\text{rods}} + \delta k_{\text{mod}} + \delta k_{\text{cool}} + \delta k_{\text{fuel}} + \delta k_{\text{Xe}} + \delta k_{\text{BU}} + \delta k_{\text{BP}} + \delta k_{\text{unk}}. \quad (10)$$

where:

- k = the excess reactivity of the core in the reference cold condition, without xenon, and at the beginning of lifetime
- k_{rods} = reactivity worth of the control rods in a just-critical reactor condition
- δk_{mod} = changes in reactivity caused by changes in the moderator temperature from the reference cold condition (68°F)
- δk_{cool} = changes in reactivity contributed by changes in the coolant gas from the reference conditions of 68°F, 1 atm, 12.2 psi
- δk_{fuel} = changes in reactivity due to fuel temperature differences from the reference 68°F
- δk_{Xe} = reactivity worth of the xenon-135 fission product

- δk_{BU} = reactivity change due to fuel burnup and fission product production (except Xe-135)
 δk_{BP} = reactivity change due to burnable poison burnup
 δk_{unk} = changes due to unknown contributions (including experimental errors)

The complete description of this equation is given in Appendix H. The first three terms on the right side of the equation represent the excess reactivity of the core at any given time, corrected for variations in moderator temperature and coolant temperature and pressure. These are easily evaluated from known relationships obtained theoretically and from previous experimentation. Figure 53 shows this quantity, and the reactor power level, plotted for all the data points. These curves clearly show the effects of xenon buildup.

Correction of the data for the effects of xenon and for the fuel temperature coefficient is more difficult since no known direct relationship exists between the experimental data and the correction. It is possible, however, to express these corrections using three proportionality constants, a, b, and c. These can be evaluated by an analysis of the experimental data. The required corrections are obtained when the proportionality constants are multiplied by terms which can be evaluated. These terms can be expressed mathematically as:

$$\delta k_{Xe} + \delta k_{fuel} = a K_{Xe} + b T_g + c P \quad (11)$$

where T_g is the coolant gas temperature, P the reactor power and K_{Xe} a function of the operating history of the reactor. To evaluate these constants, it is necessary to assume that, over a short time interval, the sum of the burnup of fuel and production of fission products plus the burnout of burnable poison plus all other unknown reactivity changes is zero. These represent the last three terms of Equation 10.

In the initial evaluation of these constants, all the data points were grouped into twelve successive time periods to satisfy the short time requirement of the analysis. The appropriate values of a, b, and c were then calculated. These constants were found to vary considerably, with the values of b and c varying in sign as well as in magnitude. The probable reason for this was found to be the relatively few data points where the fuel temperature differed from the operating value. To solve this difficulty, the data were re-grouped into four periods, each comprising a period of relatively steady state operation. The results are tabulated in the first four lines of Table 10. The reactivity changes associated with fuel temperature changes from the reference temperature to operational temperature were calculated and are shown at the right of the table.

Analysis of these results indicates that problems still exist with the use of only a few points at non-standard conditions. In a further attempt to evaluate these constants, all the data points were fitted, using the procedure described above but considering only one time period. The results obtained are shown in line 5 of the table. The reduced data developed by this method includes the effects of burnup as a displacement of the reduced curve. This information is plotted on Figure 54.

FIGURE 53a. POWER AND REACTIVITY HISTORY (REACTIVITY CORRECTED ONLY FOR MODERATOR AND COOLANT EFFECTS)

FIGURE 53b. POWER AND REACTIVITY HISTORY (REACTIVITY CORRECTED ONLY FOR MODERATOR AND COOLANT EFFECTS)

FIGURE 54. VARIATIONS IN REACTIVITY CURVE FIT INCLUDING FUEL TEMPERATURE EFFECTS

FIGURE 55. VARIATIONS IN REACTIVITY CURVE FIT WITH ONLY XENON BUILDUP CONSIDERED

TABLE 10 - CONSTANTS DETERMINED FROM REACTIVITY DATA

<u>Line</u>	<u>Period</u>	<u>a</u>	<u>b</u> <u>% $\delta k/k - F^0$</u>	<u>c</u> <u>% $\delta k/k - Mw$</u>	<u>Fuel Temperature</u> <u>Changes, % $\delta k/k$</u>
1	I	1.118	1.29×10^{-4}	-5.288×10^{-2}	-0.04
2	II	0.998	2.717×10^{-4}	-7.596×10^{-2}	+0.03
3	III	1.065	0.610×10^{-4}	-2.183×10^{-2}	-0.09
4	IV	1.035	1.166×10^{-4}	-3.320×10^{-2}	+0.01
5	only 1 period	1.021	1.290×10^{-4}	-3.252×10^{-2}	+0.03
6	only xenon	1.083	-0-	-0-	-0-

Finally, the data were analyzed assuming that the fuel coefficient was zero (b and c are zero). These results are shown on line 6 in Table 10 and in Figure 55.

3. Discussion of Results

The analyses indicate that the fuel temperature coefficient is small but cannot be determined with any certainty from the data generated during this experiment. Considering the experimental error associated with each data point ($\pm 0.06\% \delta k/k$) the curve fit with only xenon considered is as good as that obtained when the fuel temperature is included.

From the value obtained for a, the worth of equilibrium xenon at 2.2 Mw power was determined to be approximately $0.68\% \delta k/k$. Theoretical results had predicted a worth of $0.58\% \delta k/k$. No reactivity change within the limits of experimental error was noted due to burnup effects. This result agrees with the theoretical prediction; changes in the order of only a few thousands of a percent were predicted.

A review of the reactivity data indicates that immediately after the reactor power is increased, the system reactivity increases (Figures 53-55). Normally, an increase in power implies an increase in xenon which in turn implies a decrease in overall system reactivity. This discrepancy has not yet been explained.

Of the 395 data points recorded during ANSOP 16625A, 388 were taken with the three shim rods balanced; the rods were unbalanced at the time the remaining seven data points were recorded. These latter data points, numbered 204 through 210, (all occurring on 10 May 1964) provided information on the interaction between the control rod position and the power recorder reading. The pertinent information is summarized in Table 11; information from data point 211 taken with balanced shim rods is included for reference.

TABLE 11 - EFFECT OF UNBALANCED SHIM BLADE POSITIONS
ON NUCLEAR POWER CHANNELS

<u>Data Point</u>	<u>Shim Rod Position*, degrees</u>			<u>Power on Channel, %</u>		
	1	2	3	4	5	6
204	28.9	62.0	62.0	69.0	82.0	81.8
205	29.6	62.0	62.0	68.0	80.5	80.0
206	30.0	62.0	62.0	67.0	79.0	79.0
207	35.0	59.0	59.0	68.0	77.5	78.0
208	36.0	59.0	59.0	69.0	78.0	79.0
209	36.8	59.0	58.9	69.0	78.0	79.0
210	40.0	57.0	57.0	70.0	77.9	79.2
211	50.6	50.7	50.3	73.0	74.6	75.8

*Regulating rod always at 40°.

VIII. MECHANICAL PERFORMANCE

The mechanical performance of the ML-1 power plant was generally considered to be excellent. The operability of the plant was demonstrated by the successful startups following scrams experienced during the experiment. In addition, the major problems which developed during ANSOP 16625 (excessive turbine bearing wear and pressure vessel leakage) appeared to be effectively corrected. Several minor problems were encountered during the testing (including a cracked oil line fitting, leakage from the moderator to shield water system, and high break-away torque apparently due to improper functioning of the CSN-1A static seals), but none was of major consequence, and each was corrected without difficulty. The modifications made to the power plant following ANSOP 16625 were generally found to be very satisfactory as was the performance of the power plant auxiliaries.

A. REACTOR

The mechanical performance of the reactor itself was considered to be excellent. The only mechanical difficulty noted was a leak from the moderator system into the shield water system which developed after a shutdown. After investigation, it was decided to continue operation since the leak posed no hazard and moderator water could be remotely replenished. The upper moderator mechanical seal was suspected as the leak source. It was later found that the leakage virtually stopped when the reactor reached operating temperature, indicating that the leakage was related to thermal expansion. The leak was inconsequential during the remainder of the test, amounting to only about 8 gal/hr when the plant was shut down and practically zero during normal operation.

Following the test, further investigation was initiated to locate and repair the leak if possible. The leak was found to be occurring through the seal separating the moderator upper core region from the shield water. The seal was readily repaired by re-torquing the upper hold-down ring bolts which were found to be loose, presumably due to the thermal cycling.

The reactor cap was removed following ANSOP 16625A to check the instrumented fuel element in core position 5-F which had indicated higher than expected operating temperatures (see page 48). The inspection showed no apparent reasons for the indicated high temperature but did show that the new lower spider (see page 7) and the entire fuel element were in excellent mechanical condition after the many hours of operation and several thermal cycles.

B. TURBINE-COMPRESSOR SET

The mechanical operation of the CSN-1A t-c set throughout ANSOP 16625A was generally satisfactory, with the exception of a high break-away torque problem observed during one of the plant shutdown periods. The seal gas and lubrication system operating parameters held steady throughout the test, and periodic analyses of the lubricating oil did not indicate any significant bearing wear. The Bently rotor shaft displacement instrumentation also performed well during the test.

A spurious turbine overspeed scram was experienced during operation which resulted in a plant shutdown with a 29-sec measured coastdown time (as compared with the normal 35-sec coastdown time). Break-away torque was subsequently measured to be approximately 135 ft-lb, considerably higher than the normal torque (about 79 ft-lb). It was also established that the static seal system had developed a significant leak at some point within the t-c set. During a series of diagnostic checks, including cycling the static seals and hand-rotating the unit while listening for abnormal noises within the set, the break-away torque values returned to normal. It was decided, with the concurrence of Clark Bros., to resume operation but not to use the static seals since it appeared that the seals were the cause of the high torque. The plant was returned to operation and the t-c set performed satisfactorily for the remainder of the run.

The test was terminated when a power failure scram occurred on 29 May which resulted in a "hot stop". During this hot stop, the indicated turbine bearing outlet oil temperature reached 404°F (see Figure 8) while the compressor journal and thrust bearing temperatures remained below the allowable limits. Because of the high indicated turbine bearing temperature and high break-away torque (90 to 150 ft-lb) observed following the shutdown, it was decided to disassemble and inspect the CSN-1A t-c set. The unit was removed from the power conversion skid and disassembled at the NRTS by technicians from Clark Bros. The disassembly inspection revealed the following:

- 1) Both journal bearings were in good condition with no appreciable wear. The high temperature experienced by the turbine bearing during the hot stop resulted only in a darkened area due to heat discoloration (Figure 56).
- 2) There was no evidence of oil leakage through the seals into the process gas system.
- 3) The compressor and compressor inlet cavity were fouled with a slightly moist, reddish-brown deposit (Figure 57).
- 4) The compressor end static seal had rubbed on the rotor shaft and a static seal pressurization line fitting was cracked (the apparent cause of static seal leakage).
- 5) A spacer shim at the root of the first stage turbine nozzle blades had moved axially and contacted the first stage rotor blades (Figure 58). The shim had uniformly grooved the root of the turbine first stage rotor blades approximately 1/4 inch (axially) deep by 1/16 inch wide (Figure 59).

FIGURE 56. CSN-1A TURBINE JOURNAL BEARING

FIGURE 57. CSN-1A COMPRESSOR STATOR ASSEMBLY

[1.2-64-223]

FIGURE 8. CSN-1A TURBINE NOZZLE SPACER SHIM

FIGURE 59. NOTCH MACHINED IN CSN-1A FIRST STAGE TURBINE
BLADE BY SPACER SHIM

11.2-64-2233

- 6) A thermistor installed to sense turbine bearing temperature was rubbing on the rotor shaft.

Turbine-compressor set vibration during ANSOP 16625A was monitored, primarily, by the Bently displacement system which performed very well throughout the test. One turbine-end channel ceased to operate early in the run but was later repaired. The rotor vibration levels appeared to increase slightly during the run and vibration was noted at approximately one-half operating speed. The maximum displacement indicated, approximately 0.0013 in. peak-to-peak, was intermittent. The slightly higher than expected magnitude of the displacement could be attributed to:

- 1) The turbine shim ring rubbing the first stage rotor blades,
- 2) The compressor-end static seal lightly contacting the rotor, or,
- 3) The turbine bearing thermistor rubbing against the shaft.

Torsional vibration measured during the test was very similar to that recorded during ANSOP 16625 (Ref. a). The critical speeds appeared to have increased slightly as was predicted as a result of installing a stiffer shaft in the dummy alternator. The first critical speed (about 1100 rpm referred to the low speed side; about 6700 rpm turbine speed) was passed through quickly in each startup when accelerating on the start motor from quarter- to half-speed. The maximum level of vibration encountered at the first critical speed was well under the established limit (i.e., 0.3°, peak-to-peak). The second critical speed (approximately 4000 rpm; 24,400 rpm turbine speed) was beyond the operating limit of the CSN-1A, but this mode was excited to a small degree during sudden torsional load changes applied when changing the start motor speed.

Linear vibration of the t-c set case and the start motor and dynamometer were relatively stable throughout the test and the levels remained well below the established limits. The maximum vibration level indicated for the t-c set case was approximately 0.00045 in.

IX. AUXILIARIES PERFORMANCE

A. START MOTOR AND DYNAMOMETER

The ML-1 power plant with the CSN-1A t-c set does not include the prototype start motor; a 100 hp, 3-speed Elliott induction motor was used for plant start-up and post-shutdown rotation.

The start motor switch gear control settings were the same as used during ANSOP 16625 (Ref. a). These settings are optimum for acceleration from 25 to 50% speed, but are not optimum for acceleration from 50 to 100% speed. The switch gear control settings will be changed for the next test to optimize the acceleration between 50 and 100% speed.

Both the start motor and the G.E. induction dynamometer performed satisfactorily throughout the test.

B. PROCESS GAS MONITORING

1. Water, Oxygen, and Oil Vapor Monitoring

The process gas was monitored periodically for water and oxygen content and continuously for oil vapor content. The dew point of the gas was less than -100°F for the major portion of the test, indicating that the gas was extremely dry. The variation in oxygen content did not exceed the $\pm 0.2\%$ accuracy of the analysis instrumentation.

The analysis of process gas at the compressor outlet indicated no significant concentrations of oil vapor. The system for monitoring the gas for oil vapor at the desiccant tank outlet, designed to detect "break-through" of the desiccant material, revealed no significant increase in oil concentration.

2. Radioactivity Monitoring

Three systems are provided to monitor the presence of fission products in the process gas loop of the ML-1. The systems monitor delayed neutron activity and gamma activity continuously, and intermittently pass a sample of the gas through remote carbon traps for fission gas analysis. In addition, the precooler effluent (cooling air) is monitored by two Geiger-Mueller counters in a shielded carbon trap.

a. Delayed Neutron Monitor

A continuous 10 scfm flow of coolant gas is removed from the turbine inlet line, passed around two Boron-10 neutron detection chambers (located at the bottom of a 20-ft deep well, 40 ft from the reactor) and returned to the loop at the compressor inlet. This system did not function during the test. The B-10 counter outputs varied in proportion to the dynamometer excitation circuit, indicating pickup of a spurious signal.

b. Gamma Ray Monitor

Two gamma ray detectors* are located in the same water-tight canister with the two B-10 neutron detectors discussed above. The outputs of these detectors were proportional to the reactor power (Figure 60). Interruption of gas flow past the detector during steady state reactor operation resulted in a fast decay of gamma activity. This indicated the presence of short-lived isotopes, most of which was probably nitrogen-16 (7.4 sec half-life).

c. Gas Sampling Monitor

This system consists of a remote gas sampling station connected to the process gas loop by 500 ft of 1/4 in. stainless steel tubing. Gas samples from the process gas loop were periodically passed through activated carbon traps located in the remote sampling station (near the control cab). The carbon traps were subsequently analyzed in the GCRE counting room for specific gamma activity using a sodium iodide well-crystal detector and a stepwise scanning gamma spectrometer*. The total loop activity (in microcuries) is estimated from the carbon trap data using calculations programmed for computer solution (Figure 60). Gamma ray spectrometer analysis of the carbon trap activity identified xenon-133, xenon-135, iodine-131, iodine-132, and iodine-135 as the primary contributors of activity. Decay of xenon and iodine allowed the stainless steel activation products (cobalt-60, cobalt-58, iron-59, and chromium-51, and possibly, traces of silver-110) to be identified. The fission products are attributed to trace quantities of residual uranium contamination on the fuel elements as was the case during ANSOP 16625. No evidence of fuel cladding failure was observed.

d. Precooler Effluent Monitoring

Leakage of gamma-emitting fission products and other activated gases from the process gas loop is monitored by a sampling probe located in the precooler exhaust system. A 10 scfm air sample is drawn continuously through 3/4 in. stainless steel tubing to a charcoal trap located in a sealed canister under 15 ft of water shielding in the northeast corner of the test building. The carbon trap consists of 24 activated charcoal capsules to permit subsequent gamma ray spectrograph analysis. Two Geiger-Mueller counters are located in the carbon trap. This system measured essentially background radiation during the test. The carbon traps indicated no activity after the test.

*Manufactured by Tracerlab

FIGURE 60. RADIOACTIVITY MONITOR OPERATIONAL HISTORY

11.2-64-2234

C. ELECTRICAL POWER REQUIREMENTS

Auxiliary electrical power is required to operate various fans, pumps, valves, and heaters during all phases of the ML-1 power plant operation. Data collected during this test were compared with the auxiliary power loads measured during ANSOP 16625. The auxiliary load requirements for this test (ANSOP 16625A) showed no significant change from those of ANSOP 16625. In the range of t-c set speeds from 50 to 100% of rated speed, the power required varied from 54 to 63 kw.

D. NITROGEN MAKEUP SYSTEM

The nitrogen consumption during the test was determined by observing the changes in inventory at the storage bank. During the period 14 April to 30 May 1964, the total nitrogen consumption was approximately 14,234 lb.

Gross estimates of total nitrogen leakage from the gas sampling system for the overall test period are as follows:

Fission products samples	40 lb
Oxygen and humidity samples	10 lb
Hydrocarbon analysis samples	<u>3500 lb</u>
Total	3550 lb of nitrogen

In addition, a valve in the fission products gas sampling system was inadvertently left open for approximately 90 hr in early May. It is estimated that the valve exhausted approximately 3 scfm to atmosphere, equivalent to a total loss of about 1200 lb of nitrogen. Subtracting the estimated gas sampling losses (3550 lb) and valve loss (1200 lb) from the total nitrogen consumption of 14,234 lb, the net consumption was 9,484 lb of nitrogen during the 45-day test period. This is equivalent to approximately 375 standard nitrogen cylinders (220 scf per cylinder) over a 30-day period.

AGCRSP REFERENCES

1. Final Test Report - Initial Self-Sustained Operation of the ML-1 Nuclear Power Plant (ANSOP 16615), AN-AGCR-457, February 1963, Aerojet-General Nucleonics, San Ramon, California
2. ML-1 Preliminary Test Report Full Power Test - ANSOP 16625, AN-AGCR-415, 15 March 1963, Aerojet-General Nucleonics, San Ramon, California
3. ML-1-I Fuel Element - Lower Spider Modification, AN-AGCR-662, June 1964, Aerojet-General Nucleonics, San Ramon, California
4. ML-1 Reactivity Shims, AN-AGCR-675, 26 March 1964, Aerojet-General Nucleonics, San Ramon, California
5. CSN-1A Turbine Compressor Set Bearing Evaluation, Test Phase II, Clark Bros., C/T 9418, 14 April 1964
6. Data Transmittal for Air Flow Tests on ML-1 Water, Oil and Gas Coolers, OID-751, 21 January 1964
7. CSN-1A Open-Cycle Test Report, AN-AGCR-744, August 1964
8. Transmittal of Reduced and Corrected ANSOP 16625A Data, AN-AGCR-701, 28 May 1964
9. Transmittal of Remaining ANSOP 16625A Reduced Data, AN-AGCR-715, 19 June 1964
10. Method to Compute ML-1 Nominal and Hot Spot Temperatures, FED-627, 13 April 1964
11. Review of ML-1-I Hot Spot Temperature Effects, AN-AGCR-679, 6 April 1964
12. ML-1-I Re-Orificing, AN-AGCR-639, 10 January 1964

(Continued)

GENERAL REFERENCES

- a. Army Gas-Cooled Reactor Systems Program, Final Test Report Initial Full Power and Limited Endurance Tests of the ML-1 Nuclear Power Plant (ANSOP 16625), AGN TM-402, July 1963, Aerojet-General Nucleonics, San Ramon, California
- b. Army Gas-Cooled Reactor Systems Program, ML-1 Pressure Vessel Repair Summary Report, AGN TM-405, December 1963, Aerojet-General Nucleonics, San Ramon, California
- c. Army Gas-Cooled Reactor Systems Program, Quarterly Progress Report, 1 July Through 30 September 1963, IDO-28617, 15 November 1963, Aerojet-General Nucleonics, San Ramon, California
- d. Army Gas-Cooled Reactor Systems Program, Quarterly Progress Report, 1 January Through 31 March 1964, IDO-28626, 15 May 1964, Aerojet-General Nucleonics, San Ramon, California
- e. Army Gas-Cooled Reactor Systems Program, CHOP, A Computer Program for Calculating Steady State Thermodynamic Performance, AGN TM-388, May 1963
- f. Franklin Institute, "Thermodynamic Properties of Helium and Molecular Nitrogen", Preliminary Draft
- g. Army Gas-Cooled Reactor Systems Program, HECTIC-II, An IBM 7090 Fortran Computer Program for Heat Transfer Analysis of Gas or Liquid Cooled Reactor Passages, IDO-28595, December 1962, Aerojet-General Nucleonics, San Ramon, California
- h. Army Gas-Cooled Reactor Systems Program, HECTIC, An IBM 704 Computer Program for Heat Transfer Analysis of Gas-Cooled Reactors, AGN TM-381, May 1961, Aerojet-General Nucleonics, San Ramon, California
- i. Army Gas-Cooled Reactor Systems Program, The ML-1 Design Report, IDO-28550, 16 May 1960, Aerojet-General Nucleonics, San Ramon, California
- j. Army Gas-Cooled Reactor Systems Program, ML-1 Shielding Design Report, IDO-28609, October 1963, Aerojet-General Nucleonics, San Ramon, California

APPENDIX A

AGCRSP BACKGROUND INFORMATION

APPENDIX A

AGCRSP BACKGROUND INFORMATION

This background of the Army Gas-Cooled Reactor Systems Program includes a short history of the Program, a description of the ML-1 power plant, and a selected bibliography.

A. HISTORY

The purpose of the Army Gas-Cooled Reactor Systems Program (AGCRSP) is to develop a mobile nuclear power plant for military field use. The current primary goal of the Program is the fabrication and test operation of a demonstration model of such a plant.

In 1955, at the request of the USAEC Division of Reactor Development, the Oak Ridge School of Reactor Technology performed a study which established the feasibility of the concept of a mobile, closed-cycle, gas-cooled nuclear power plant. Following this work, the Corps of Engineers Nuclear Power Field Office authorized the Sanderson and Porter Company to evaluate power conversion equipment and to prepare a conceptual design for the projected plant. At the conclusion of the Sanderson and Porter work, responsibility for development of the power conversion equipment for the plant was assigned to the Corps of Engineers and the development of the reactor was assigned to the USAEC.

As a result of the above arrangement, parallel programs were undertaken as follows:

- 1) The Corps of Engineers directed the Stratos Division of Fairchild Engine and Aircraft Corporation to develop a turbine-compressor set suitable for use in the projected plant. The construction of a test facility (GTTF) to evaluate the power conversion equipment was assigned to Aerojet-General Corporation. (The design of the facility was completed by Sanderson-Porter.)
- 2) Under the direction of the USAEC, Aerojet studied the feasibility of several concepts for the reactor to be incorporated in the power plant. The water-moderated concept was selected as the basis for development because of the modest extrapolation of technology required. Aerojet was

awarded the contract for the design and testing of a reactor based on this concept. At the same time, Aerojet was assigned responsibility for the design and construction of a test facility (GCRE) at NRTS.

Aerojet was designated as systems contractor and performance specifications for the demonstration power plant were evolved in June 1959. Fabrication of the reactor skid was completed in April of 1961, fabrication of the power conversion skid was completed in June 1962, and the power plant first operated as a unit (ML-1) in September 1962. Following a modification and checkout, the ML-1 power plant operated successfully for 101 hours in February and March 1963.

B. THE ML-1

The ML-1 is a closed cycle, gas-cooled nuclear power plant developed under the AGCRSP to demonstrate the feasibility of such a plant for military field use. During the design and construction, every reasonable effort was made to incorporate features into the plant which would be directly usable in the design of a field unit. However, since evaluation of the performance of the plant was a major requirement for the ML-1, a significant amount of additional instrumentation was provided. The physical arrangement of the equipment is such that the "prototype" components are easily identified as the:

- 1) Reactor Skid - a 15 ton unit containing the nuclear reactor and associated shielding and controls;
- 2) Power Conversion Skid - a 15 ton unit containing the power conversion equipment; and,
- 3) Control Cab - a 2-1/2 ton unit containing all the instruments and controls for operation of the field plant.

The ML-1 reactor consists of a calandria-type pressure vessel with appropriate inlet and exit gas ducts and plenums. Sixty-one pin-type BeO-UO_2 fuel elements are located in the tubes of the calandria. The demineralized water moderator surrounds the calandria and the six semaphore-type control rods operate in the moderator spaces between the calandria tubes. The entire reactor structure is supported inside a nine-foot diameter tank which contains heavy metal shields to permit relocation of the reactor within 24 hours after shutdown from extended operation, and a drainable (borated water) shield to attenuate radiation during reactor operation.

The plant working fluid (99.5 vol% nitrogen, 0.5 vol% oxygen) enters the reactor at 800°F and approximately 300 psia. The gas is heated to 1200°F in a single pass over the hot surfaces of the fuel elements. The moderator water is maintained at a temperature of 180°F ; energy deposited in the moderator is removed in an water-to-air heat exchanger mounted on top of the power conversion skid. Provision is made for circulation, filtration and demineralization of the moderator water and for circulation and cooling (by exchange with the moderator water) of the shield water.

The hot gas leaving the reactor is expanded in a gas turbine which drives the compressor and alternator. The gas leaving the turbine passes through a regenerative heat exchanger (recuperator), through the system heat sink (air-to-air precooler) and to the compressor suction. The compressor discharges through the recuperator to the reactor inlet, thus completing the closed (modified Brayton) cycle. A lubrication system (including provision for recovery and removal of lubricating oil from the working fluid which leaks past the turbine compressor seals), the electrical switch gear, and miscellaneous power conversion hardware are mounted on the power conversion skid. An a-c, two-speed motor is coupled to the turbine compressor shaft to provide starting power to the set.

The following auxiliary systems are provided for the ML-1:

- 1) A deoxygenation system which removes dissolved oxygen from a bypass stream to maintain the moderator system oxygen content below 0.7 ppm.
- 2) An emergency cooling system which automatically injects a supply of coolant gas into the reactor in the event of a complete stoppage of working fluid flow.
- 3) A working fluid makeup system to compensate for normal leakage and to provide for initial charging of the system.
- 4) Waste gas storage facilities to accommodate the charge of radioactive gas in the loop in the event of an emergency.

C. BACKGROUND BIBLIOGRAPHY

The following bibliography provides information on sources of additional background to the material presented in this report. These documents trace the technical evolution of the AGCRSP from inception but do not, in general, document programmatic decisions. Such activity may be inferred from the technical approaches pursued and from the general background information presented in the reports.

The following reports were published by Aerojet-General Nucleonics, San Ramon, California under the Army Gas-Cooled Reactor Systems Program.

<u>DOCUMENT NO.</u>	<u>TITLE</u>	<u>CLASSIFICATION</u>
IDO-28505	<u>GCRE Semiannual Report, 1 November 1956 Through 30 June 1957, 20 February 1958</u>	CRD
IDO-28506	<u>GCRE-I Hazards Summary Report, December 1958, with three addenda, March 1959, February 1960, May 1960</u>	U
IDO-28519	<u>GCRE Semiannual Progress Report, 1 July Through 31 December 1957, 26 June 1958</u>	CRD
IDO-28526	<u>GCRE Semiannual Progress Report, 1 January Through 30 June 1958, 17 October 1958</u>	CRD

DOCUMENT NO.	TITLE	CLASSIFICATION
IDO-28533	<u>AGCRSP Semiannual Progress Report, 1 July Through 31 December 1958, 28 February 1959</u>	CRD
IDO-28537	<u>Preliminary Hazards Summary Report for the ML-1 Nuclear Power Plant, 22 April 1959</u>	U
IDO-28542	<u>AGCRSP Semiannual Progress Report, 1 January Through 30 June 1958, July 1958</u>	U
IDO-28549	<u>AGCRSP Semiannual Progress Report, 1 July Through 31 December 1959, 22 December 1959</u>	U
IDO-28550	<u>The ML-1 Design Report, 16 May 1960</u>	U
IDO-28555	<u>ML-1 Transportability Studies, 23 March 1960</u>	U
IDO-28558	<u>AGCRSP Semiannual Progress Report, 1 January Through 30 June 1960, 11 July 1960</u>	U
IDO-28560	<u>Final Hazards Summary Report for the ML-1 Nuclear Power Plant, with four supplements, 5 August 1960</u>	U
IDO-28567	<u>AGCRSP Semiannual Progress Report, 1 July Through 31 December 1960, 17 December 1960</u>	U
IDO-28573	<u>AGCRSP Semiannual Progress Report, 1 January Through 30 June 1961, 10 August 1961</u>	U
IDO-28581	<u>AGCRSP Semiannual Progress Report, 1 July Through 31 December 1961, 31 January 1962</u>	U
IDO-28590	<u>AGCRSP Semiannual Progress Report, 1 January Through 30 June 1962, 24 August 1962</u>	U
IDO-28597	<u>AGCRSP, Study of the GCRE Tube Bundle Failure, 14 December 1962</u>	U
IDO-28602	<u>AGCRSP Semiannual Progress Report, 1 July Through 31 December 1962, 22 February 1963</u>	U
IDO-28607	<u>AGCRSP Quarterly Progress Report, 1 January Through 31 March 1963, 15 May 1963</u>	U
IDO-28612	<u>AGCRSP Quarterly Progress Report, 1 April Through 30 June 1963, 15 August 1963</u>	U
IDO-28617	<u>AGCRSP Quarterly Progress Report, 1 July Through 30 September 1963, 15 November 1963</u>	U

<u>DOCUMENT NO.</u>	<u>TITLE</u>	<u>CLASSIFICATION</u>
IDO-28621	<u>AGCRSP Quarterly Progress Report, 1 October</u> <u>Through 31 December 1963, 27 January 1964</u>	U
IDO-28626	<u>AGCRSP Quarterly Progress Report, 1 January</u> <u>Through 31 March 1964, 15 May 1964</u>	U
IDO-28632	<u>AGCRSP Quarterly Progress Report, 1 April</u> <u>Through 30 June 1964, 15 August 1964</u>	U

APPENDIX B

ML-1 INSTRUMENTATION LIST

APPENDIX B

ML-1 INSTRUMENTATION LIST

The following pages list the ML-1 instrumentation, and identifies the location on the P & I Diagram (Figure B-1), defines the sensor, readout instrument, range calibration range, nominal values, and errors. In addition, each instrument is categorized as either primarily for comparison with theoretical values (Class 1), or for use in controlling the power plant (Class 2).

FIGURE B-1. ANALYSIS INSTRUMENTATION SENSOR LOCATIONS ML-1

PRIMARY LOOP TEMPERATURE INSTRUMENTATION

(NOTE: P & I No. refer to Figure B-1)

Class*	P & I No.	Location	Sensor	Depth, in.	Dia. in.	Readout Instrument	Temperature, °F			
							Range	Calib. Range	Nominal	Error
1	TR-102-2	Compressor Inlet	C/A Thermo-couple	4.2	10.12	Bristol Recorder	0-600 Δ = 25	25-150	133	+5.9 -5.9
1	TR-102-3	↓	C/A Thermo-couple	5.0	↓	Bristol Recorder	0-1000 Δ = 25	25-150 Δ = 22	↓	+6.9 -6.9
2	TI-102-1	↓	Tungsten Resist. Bulb	2.25	↓	Westinghouse Meter (cab)	-50 +200	25-150	↓	+7.0 -2.2
1	TX-102-1	↓	C/A Thermo-couple	2.25	↓	Houston Voltmeter	----	25-120	↓	+4.5 -4.5
1	TXR-111	Bypass Line	↓	1.1	2.25	Bristol Recorder, Houston Voltmeter	0-1000 ----	62-300 Δ = 29	370	+7.5 -7.5 +5.3 -5.3
1	TXR-115-1	Compressor Outlet	↓	3.8	8.0	Bristol Recorder, Houston Voltmeter	0-1000 ----	62-300 Δ = 29	370	+7.5 -7.5 +5.3 -5.3
1	TXR-115-2	Compressor Outlet	↓	2.8	8.0	Bristol Recorder, Houston Voltmeter	0-1000 ----	62-300 Δ = 29	370	+7.5 -7.5 +5.3 -5.3
1	TX-109-1	Reactor Inlet	↓	2.0	10.25	Bristol Recorder (fuel element) Houston Voltmeter	0-2000	62-850 Δ = 87	800	+12 -12 +8.8 -8.8
1	TX-109-2	Reactor Inlet	↓	↓	10.25	Bristol Recorder (fuel element) Houston Voltmeter	0-2000	62-850 Δ = 87	↓	+12 -12 +8.8 -8.8
2	TX-101-1	Reactor Outlet (Turbine Inlet)	↓	↓	8.65	Houston Voltmeter	----	62-1250	↓	+12 +12
1	TX-101-2	↓	↓	↓	↓	Brown Recorder (R-5702) (1 pt.)	0-1500	----	1200	+13 -13
1	TX-101-3	↓	↓	↓	↓	Brown Recorder (R-5702) (1 pt.)	0-1500	----	↓	+13 -13
1	TR-101-4	↓	↓	↓	↓	Bristol Recorder (fuel element)	0-2000	62-1250 Δ = 85	↓	+14 -14
1	TXI-101-6	↓	↓	↓	↓	Recorder (Analysis) Opt.	0-2000	----	↓	----
1	↓	↓	↓	↓	↓	Houston Voltmeter	----	----	↓	+13 -13
2	↓	↓	↓	↓	↓	Howell Indicator (cab)	400-1400	----	↓	+19 -19
1	TR-101-8	↓	↓	↓	↓	Bristol Recorder (R-5802)	0-2000	62-1250 Δ = 85	↓	+15 -15
1	TR-101-9	↓	↓	↓	↓	Bristol Recorder (R-5802)	0-2000	62-1250 Δ = 85	↓	+15 -15
1	TR-101-5	↓	↓	6.0	↓	Houston Voltmeter	----	62-1250 Δ = 85	↓	+13 -13
2	TIC-101-1	Reactor Outlet	Plat. Resist. Bulb	6.0	↓	Westinghouse Meter, Cab	400-1400	----	↓	+20 -15
1	TXR-110-1	Turbine Outlet	C/A Thermo-couple	---	---	Bristol Recorder (R-5802) Houston Voltmeter	0-2000 ----	62+1000 Δ = 45	900	+13 -13 +10 -10
1	TXR-110-3	Turbine Rake	↓	---	9.8-162	Bristol Recorder (fuel element) Houston Voltmeter	0-2000 0-2000	62+1000 Δ = 45	↓	↓
1	TXR-110-4	Turbine Rake	↓	---	9.8-162	Bristol Recorder (fuel element) Houston Voltmeter	↓	↓	↓	↓
1	TXR-110-5	↓	↓	---	9.8-162	Bristol Recorder (fuel element) Houston Voltmeter	↓	63+1000 Δ = 45	↓	↓
1	TR-112-1	L.P. Recup. Out.	↓	3.8	13.0	Bristol Recorder Houston Voltmeter	0-1000 ----	62+500 Δ = 45	490	+7.5 -7.5 +5.7 -5.7
1	TR-112-2	L.P. Recup. Out.	↓	3.8	13.0	Bristol Recorder Houston Voltmeter	0-1000 ----	62+500 Δ = 45	490	+7.5 -7.5 +5.7 -5.7

*Class 1 - Primary Analysis Instrumentation; Class 2 - Secondary Analysis (Field) Instrumentation

MODERATOR LOOP TEMPERATURE INSTRUMENTATION

Class*	P & I No.	Location	Sensor	Depth, in.	Dia. in.	Readout Instrument	Range	Temperature, °F			
								Calib. Range	Nominal	Error	
1	TDX-205-1 NPQ-31	Mod. W. ΔT	Cu/Con Thermocouple	---	4.0	Houston Voltmeter	---	0-15 $\Delta=5$	4.5	+0.5	-.05
1	TDX-205-2	Mod. W. ΔT	Cu/Con Thermocouple	---	4.0	Houston Voltmeter	---	0-15 $\Delta=5$	4.5	+0.5	-.05
1	TR-202-1	Mod. W. Out. Rx	C/A Thermo- couple	---	4.0	Bristol Recorder R-5502	0-600	130-190 $\Delta=12$	190	+5.9	-5.9
2	TI-202-4	Mod. W. Out. Rx	Tunst.Resist. Bulb	1.25	4.0	Westinghouse Meter Cab	0-250	130-190 $\Delta=12$	190	+7.0	-2.2
2	TI-201-4	Mod. W. In Rx	Tunst.Resist. Bulb	1.25	4.0	Westinghouse Meter Cab	0-250	130-190 $\Delta=12$	190	+7.0	-2.2

AMBIENT AIR TEMPERATURE INSTRUMENTATION

1	TR-155	Precooler Air Inlet	C/A Thermo- couple	---	---	Bristol Recorder R-5502	0-600	25-125 $\Delta=25$	0-100	+5.9	-5.9
1	TR-156			---	---	Bristol Recorder R-5502	0-600			+5.9	-5.9
1	TR-157			---	---	Houston Voltmeter	---			+4.4	-4.4
1	TSR-158			---	---	Recorder R-51003 Houston Voltmeter	0-600 ---				
1	TXR-159			---	---	Bristol Recorder R-5502 Houston Voltmeter	0-600 ---				
1	TXR-160			---	---	Recorder R-51003 Houston Voltmeter	0-600 ---		0-100	+5.9 +4.4	-5.9 -4.4

LUBRICATION OIL LOOP TEMPERATURE INSTRUMENTATION

1	TR-616-1	Lube Oil to Cooler	C/A Thermo- couple	---	---	Bristol Recorder R-5502	0-600	120-165 $\Delta=30$	160	+4.9	-4.9
1	TR-619-1	Lube Oil From Cooler	C/A Thermo- couple	---	---	Bristol Recorder R-5502	0-600	120-150 $\Delta=15$	140	+4.9	-4.9

PRIMARY GAS LOOP PRESSURE INSTRUMENTATION

Class*	P & I No.	Location	Sensor	Readout Instrument	Instrument Range, psia	Calib. Range	Scale Read ΔP , psia	Nominal Pres- sure, psia	Error, psia**	
									Gauge System	
1	PI-120	Compressor In- let	---	Barton	0-150	55-125 psia $\Delta=10$ psia	± 0.5	116	± 0.88	± 5.4
1	PI-121	Compressor Out- let	---	Barton	0-350	60-330 $\Delta=30$	2.5	320	± 3.4	± 14
1	PI-123	Recuperator Out- let H.P.	---	Barton	0-350	60-30 $\Delta=30$	2.5	314	± 3.4	± 14
1	PI-124	Turbine Inlet	---	Barton	0-350	60-300 $\Delta=30$	2.5	290	± 3.4	± 14
1	PI-125	Turbine Outlet	---	Barton	0-150	55-130 $\Delta=15$	0.5	120	± 0.88	± 5.4

* Class 1 - Primary Analysis Instrumentation; Class 2 - Secondary Analysis (Field) Instrumentation

**Right hand column includes $\pm 3\%$ Barton Temperature Compensation Error

PRESSURE DIFFERENTIAL INSTRUMENTATION

<u>Class*</u>	<u>P & I No.</u>	<u>Location</u>	<u>Sensor</u>	<u>Readout Instrument</u>	<u>Instrument Range</u>	<u>Calib. Range</u>	<u>Scale Read Δ P</u>	<u>Nominal Pressure</u>	<u>Error, psia**, Gauge System</u>	
1	PdI-130	P ₁₋₂ Compressor	---	Barton	0-200 psid	0-200 psid $\Delta=20$ psid	0.5 psid	200 psid	± 1.0	± 7.2 psid
1	PdI-132	P ₄₋₆ Reactor	---	Barton	0-25 psid	0-20 psid $\Delta=4$	0.25 psid	23 psid	± 0.31	± 1.1 psi
1	PdI-135	P ₂₋₇ Compr. Out-let Turbine Inlet	---	Barton	0-50 psid	0-25 psid $\Delta=5$	0.25 psid	1.5-6.0 psid	± 0.38	± 1.9 psi
1	PdI-136	P ₈₋₁ Turbine Out-let Compr. Inlet	---	Barton	0-10 psid	0-6 psid $\Delta=1$	0.5 psid	3.8 psid	± 0.075	± 0.38 psi
1	PdI-139	P ₆₋₇ Reactor Out-let Turbine Inlet	---	Barton	0-50 in. H ₂ O	0-50 in. $\Delta=10$	0.25 in.	16 in.	± 0.38	± 1.9 in.
1	PdI-137	P ₉₋₁₀ Recuperator LP	---	Barton	0-50 in.	0-50 in. $\Delta=10$	0.25 in.	23 in.	± 0.38	± 1.9 in.
1	PdI-133	P ₁₁₋₁₂ Precooler	---	Barton	0-10 psid	0-5 psid $\Delta=1$	0.05 psid	1.7 psid	± 0.075	± 0.38 psi
1	PdI-134	P ₁₁₋₁₂ Precooler	---	Barton	0-150 in.	0-150 in. $\Delta=50$ in.	0.5 in.	47 in.	± 0.88	± 5.4 in.

FLOW RATE INSTRUMENTATION

<u>Class*</u>	<u>P & I No.</u>	<u>Location</u>	<u>Sensor</u>	<u>Readout Instrument</u>	<u>Instrument Range</u>	<u>Calib. Range</u>	<u>Scale Read Δ P</u>	<u>Nominal Pressure</u>	<u>Gauge System Error Range, in.</u>		<u>Gauge System Error Range, %**</u>		<u>Estimated, % Total Error**</u>	
1	FI-140-1	Compressor In-let Flow	Pitot Tube	Barton	0-50 in.	0-50 $\Delta=16$	0.25 in.	31 in.	± 0.38	± 1.9	± 0.60	± 3.1	± 10.0	± 11.0
1	FI-141	Bypass Flow	Venturi Taps	Barton	0-150 in.	0-150 $\Delta=50$	0.50 in.	35 in.	± 0.90	± 5.4	1.3	8.0	3.5	11.0
1	FI-142	Turbine Inlet	Venturi Taps	Barton	0-100 in.	0-70 in. $\Delta=10$	0.50 in.	60 in.	± 0.80	± 3.8	0.67	3.2	1.9	4.4
2	FI-601	7th Stage Seal Gas Flow	Venturi-Statham	Houston Voltmeter	0-50 in.	0-50 in. $\Delta=16$	± 0.25 in.	30 in.	± 6	-6	10.0	10.0	14.0	14.0
1	FI-601	7th Stage Seal Gas Flow	Venturi-Taps	Barton	0-25 in.	0-50 in. $\Delta=16$	± 0.25 in.	30 in.	± 0.31	± 1.0	0.05	1.7	10.0	10.0

MODERATOR SYSTEM INSTRUMENTATION

1	FI-202	Moderator W. Flow	P _{reactor} Press. Taps	Barton Δ P (199)	0-30 psid	12-20 psid $\Delta=2$ psid	0.25 psid	15 psid	± 0.32 psid	± 0.45 psid	1.0	1.5	1.8	2.3
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LUBRICATION OIL SYSTEM INSTRUMENTATION

1	FI-621	Oil Cooler Inlet	Orifice 0.730 dia	Barton Δ P	0-50 psid	0-50 psid $\Delta=16$ psid	0.25 psid	10 psid	± 3.8	± 1.9 psid	1.9	10.0	5.4	11.0
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* Class 1 - Primary Analysis Instrumentation; Class 2 - Secondary Analysis (Field) Instrumentation

**Includes Barton gauge temperature compensation error.

APPENDIX C

STATISTICAL PROBABILITIES OF MEASUREMENTS

NOTATION

P = probability distribution
H = enthalpy
A = recuperator shell loss
 W_c = compressor flow
 W_t = turbine flow
B = thermodynamic output power

SUBSCRIPTS:

i = location as defined in Figure 9, main text
j = individual measurement at location

APPENDIX CSTATISTICAL PROBABILITIES OF MEASUREMENTS

The criteria used for the determination of "mean" values from sets of experimental data were the laws of statistics and probability. Two basic methods were used:

- 1) The statistical averaging process to determine statistical average values, and,
- 2) The statistical principle of maximum likelihood involving two equations of restraint to determine most probable values.

In the two methods, the common assumptions were made that the error in each measurement is normally distributed about a mean temperature with a specified variance (expected error), and that the errors of the individual measurements were independently distributed.

The statistical average value of a series of measurements ($M_{ij}, j=1,2,3,\dots,n$) at some location i were determined by the equation

$$\bar{M}_i \pm \bar{\sigma}_i = \frac{\sum_{j=1}^n \frac{M_{ij}}{\sigma_{ij}^2}}{\sum_{j=1}^n \frac{1}{\sigma_{ij}^2}} \pm \sqrt{\frac{1}{\sum_{j=1}^n \frac{1}{\sigma_{ij}^2}}} \quad (C-1)$$

where

- $\bar{M}_i$ = statistical average of the measurements at state point i
- $\bar{\sigma}_i$ = variance of $\bar{M}_i$
- M_{ij} = j th measurement at location i
- σ_{ij} = variance of M_{ij}

However, determination of the most probable values of sets of related measurements required a more involved approach. From the assumption of the distributed measurements, the distribution of the M_{ij} 's were given by

$$P_j(M_{ij}) = \frac{1}{\sigma_{ij}\sqrt{2\pi}} e^{-\frac{1}{2} \left(\frac{M_{ij} - \bar{M}_i}{\sigma_{ij}} \right)^2} \quad (C-2)$$

The product $\prod P_j(M_{ij})$ gave the probability distribution of the entire set of measurements L , where

$$L = \prod_{ij} P_j(M_{ij}) = \frac{1}{\prod_{ij} \sigma_{ij} (\sqrt{2\pi})^n} e^{-\frac{1}{2} \sum_{ij} \left(\frac{M_{ij} - \bar{M}_i}{\sigma_{ij}} \right)^2}$$

where $n = \sum_{i=1}^I J_i$

I = value to which i 's are summed

J_i = value to which j 's are summed

The statistical principle of maximum likelihood states that the value of $\bar{M}_i$ be selected to maximize L . However, the $\bar{M}_i$'s were subject to the following additional constraints:

$$\phi_1 = f(M_i) = 0 \quad (C-3)$$

$$\phi_2 = f'(M_i) = 0 \quad (C-4)$$

Consequently, the function L was required to be maximized with respect to Equations C-3 and C-4 by the use of Lagrange multipliers (λ_1, λ_2). This requirement gave the following equation for each i :

$$\frac{\partial L}{\partial M_i} + \lambda_1 \frac{\partial \phi_1}{\partial M_i} + \lambda_2 \frac{\partial \phi_2}{\partial M_i} = 0 \quad (C-5)$$

resulting in $I + 2$ equations with $I + 2$ unknowns.

For the code DRML-1*, the two equations of restraint were the t-c set and recuperator energy balances:

*DRML-1: An unpublished code written at AGN.

$$\phi_1 = H_8 - H_{10} - H_4 + H_2 - A = 0 \quad (C-6)$$

$$\phi_2 = H_7 - H_8 - \frac{W_c}{W_t} H_2 + \frac{W_c}{W_t} H_1 - \frac{1.055}{W_t} B = 0 \quad (C-7)$$

For the system measurements, the number of locations was $I = 6$, with the following number of measurements at each location, $J_1 = 1$, $J_2 = 2$, $J_4 = 2$, $J_7 = 4$, $J_8 = 2$, $J_{10} = 1$. Evaluation of Equation C-5 for each location resulted in the equations:

$$\frac{H_1 - H_{11}}{2} + \frac{H_1 - H_{12}}{2} + \frac{W_c}{W_t} \frac{\lambda_2}{(-L)} = 0 \quad (C-8)$$

$$\frac{H_2 - H_{21}}{2} + \frac{H_2 - H_{22}}{2} + \frac{\lambda_1}{(-L)} - \frac{W_c}{W_t} \frac{\lambda_2}{(-L)} = 0 \quad (C-9)$$

$$\frac{H_4 - H_{41}}{2} + \frac{H_4 - H_{42}}{2} - \frac{\lambda_1}{(-L)} = 0 \quad (C-10)$$

$$\frac{H_7 - H_{71}}{2} + \frac{H_7 - H_{72}}{2} + \frac{H_7 - H_{73}}{2} + \frac{H_7 - H_{74}}{2} + \frac{\lambda_2}{(-L)} = 0 \quad (C-11)$$

$$\frac{H_8 - H_{81}}{2} + \frac{H_8 - H_{82}}{2} + \frac{\lambda_1}{(-L)} = 0 \quad (C-12)$$

$$\frac{H_{10} - H_{101}}{2} - \frac{\lambda_1}{(-L)} = 0 \quad (C-13)$$

Equations C-6 through C-13 constitutes a set of eight linear equations in eight unknowns and yields the most probable values of enthalpies consistent with the variables σ_{ij} and constraints ϕ_1, ϕ_2 . It should be noted that $\frac{\lambda_1}{(-L)}$ and $\frac{\lambda_2}{(-L)}$ are considered as unknowns but have no other significance in the solution of the problem.

APPENDIX D

CALCULATION OF RELATIVE PRESSURES

APPENDIX DCALCULATION OF RELATIVE PRESSURES

The relative pressure method of determining the isentropic state points is shown in Keenan and Kayes (Reference D-1) which contains tables of relative pressures based on atmospheric pressures. A similar set of tables may be derived using gas properties from a Franklin Institute paper (Reference D-2) for a base pressure of 225 psia, the average system design pressure of the ML-1.

The derivation, extracted in part from Keenan and Kayes is as follows:

For a homogeneous system, changes between equilibrium states may be written as:

$$Tds = dh - \frac{v}{J} dp. \quad (D-1)$$

where

T = temperature, °R

S = entropy, Btu/lb-°R

h = enthalpy, Btu/lb

v = specific volume, ft³/lb

p = pressure, lb/ft²

J = mechanical equivalent of heat, ft-lb/lb-Btu.

If h is assumed to be a function of T only,

$$Tds = C_p dt - \frac{v}{J} dp \quad (D-2)$$

where

C_p = specific heat at constant pressure, Btu/lb-°F.

For an infinitesimal step in an isentropic process

$$Tds = 0 = C_p dT - \frac{v}{J} dp. \quad (D-3)$$

Assuming ideal gas relations apply

$$0 = C_p dT - \frac{RT}{P} dp \quad (D-4)$$

or

$$\frac{dp}{P} = \frac{J}{R} C_p \frac{dT}{T} \quad (D-5)$$

where

$$R = \text{gas constant, ft-lb/lb-}^{\circ}\text{F}$$

The specific heat is assumed to be a function of T only, therefore:

$$\ln \frac{P}{P_o} = \frac{J}{R} \int_{T_o}^T C_p \frac{dT}{T} \quad (D-6)$$

When a base temperature T_o is selected, the ratio $\frac{P}{P_o}$, defined as the relative pressure at T, becomes a single function of the temperature T regardless of the value of entropy.

Since Equation D-6 is dependent only on temperature, the integral may be evaluated if C_p is consistent with the original stipulation of evaluating gas properties at 225 psia. Therefore, Equation D-2 may be used to represent the integral as a simple function of gas thermodynamic properties as follows:

For a constant pressure process:

$$T ds = C_p dT \quad (D-7)$$

or

$$ds = C_p \frac{dT}{T} \quad (D-8)$$

Integrating

$$S - S_o = \int_{T_o}^T \frac{C_p}{T} dT \quad (D-9)$$

Therefore, by arbitrarily selecting the base temperature, the integral may be evaluated by merely determining the entropy difference at 225 psia between the base temperature and the temperature required. Thus, from the definition of relative pressure

$$Pr = \frac{P}{P_o} = e^{\frac{J}{R} \int_{T_o}^T C_p \frac{dT}{T}} \quad (D-10)$$

or

$$Pr = e^{\frac{J}{R} (S - S_o)}$$

REFERENCES

- D-1 Keenan & Kayes, Gas Tables, John Wiley & Sons, October 1957
- D-2 Franklin Institute, "Thermodynamic Properties of Helium and Molecular Nitrogen," Preliminary Draft

APPENDIX E
CALCULATION OF DUCT THERMAL LOSSES
AND
THERMOCOUPLE RADIATION LOSS ERRORS

NOTATION

A	=	heat transfer area, ft ²
C	=	thermocouple probe perimeter, ft
C _p	=	specific heat of gas, Btu/lb-°F
D _o	=	gas duct diameter, ft
G	=	mass flow rate divided by flow area, lb/ft ²
h	=	heat transfer coefficient, Btu/hr-ft ² -°F
K	=	thermal conductivity, Btu/hr-ft-°F
L	=	gas duct length, ft
l	=	thermocouple probe length, ft
m	=	hC/KA, 1/ft
q	=	heat flow rate, Btu/hr
T _a	=	ambient temperature, °F
T _g	=	gas temperature, °F
T _{in}	=	gas inlet temperature, °F
T _p	=	thermocouple probe temperature, °F
T _w	=	duct wall temperature, °F
ΔT	=	T _g - T _w
ΔT _f	=	gas duct film drop, °F
ΔT _{error}	=	thermocouple measuring error
W	=	coolant flow rate, lb/hr
U	=	overall heat transfer conductance of gas duct, Btu/hr-ft ² -°F
ε	=	emissivity
σ	=	0.17 x 10 ⁻⁹ Btu/hr-ft ² -°R ⁴
μ	=	viscosity, lb/hr-ft
DLU	=	diameter, length, overall heat transfer coefficient

APPENDIX E
CALCULATION OF DUCT THERMAL LOSSES
AND
THERMOCOUPLE RADIATION LOSS ERRORS

INTRODUCTION

An analysis of thermocouple error due to thermal radiation was conducted using the computer code KIM*. The thermocouples in the reactor inlet and outlet ducts and turbine outlet flange were studied. The thermocouples located in the precooler inlet and outlet and compressor inlet and outlet were not studied since the coolant in these areas are below 500°F, thus radiation heat transfer should be negligible.

DISCUSSIONHeat Loss and Radiation

The computer code performs a heat balance on the thermocouple probe by balancing the heat gained from convection from the gas and loss by thermal radiation to the cooler gas duct wall.

Heat gained from the gas

$$q = hA \Delta T_f \quad (E-1)$$

$$h = 0.24 \frac{K}{Do} \left(\frac{Do G}{\mu} \right)^{.6} \quad (\text{Ref. E-1, p. 260}) \quad (E-2)$$

Heat loss to the gas duct

$$q = \sigma \epsilon (T_p^4 - T_w^4) \quad (E-3)$$

*Computer code to be documented

The gas duct wall temperature was determined from the heat transfer coefficient computed for the duct and the overall conductance to ambient based on the heat loss values obtained earlier*.

$$T_w = T_g - \frac{U}{h} (T_g - T_a) \quad (E-4)$$

The code also computes the gas temperature drop along the duct as it loses heat to ambient.

$$T_g = T_a + (T_{in} - T_a) \exp \left(\frac{DLU}{wCp} \right) \quad (E-5)$$

The radiation thermocouple error of the reactor outlet probe is plotted on Figure E-1. The gas temperature drop to each thermocouple location is plotted on Figures E-2 through E-4. The radiation error is the same for all three thermocouples at the three locations. Thus, to predict the reactor outlet temperature from the thermocouple readings, the corrections for radiation and duct heat loss must be added to the measured heat loss. At the design condition, the probe heat transfer coefficient was 446 Btu/hr-ft²-°F and the duct film drop was 36°F. At 10% flow rate and design temperature, the heat transfer coefficient and duct film temperature drop were 112 Btu/hr-ft²-°F and 190°F.

The reactor inlet thermocouple error due to radiation and coolant heat loss from point of measurement to core inlet were determined from two separate computer runs (Figure E-5). At the design condition, the probe heat transfer coefficient was 387 Btu/hr-ft²-°F and the duct film temperature drop was 16°F. At 10% flow rate and design temperatures, the heat transfer coefficient and film temperature drop were 97 Btu/hr-ft²-°F and 92°F.

The turbine outlet thermocouple radiation error is plotted on Figure E-6. The probe is installed downstream of the turbine outlet diffuser where the gas must make a 90° turn around the baffle in the recuperator. The equivalent diameter at the thermocouple was computed as 12.2 in. At the design condition the probable heat transfer coefficient was 228 Btu/hr-ft²-°F and the recuperator film temperature drop was 30°F. At 10% flow rate and design temperature the heat transfer coefficient and film drop were 59 Btu/hr-ft²-°F and 70°F.

*Internal communication: AN-AGCR-610

Inputs

The emissivity of the stainless steel thermocouple probe was taken as 0.8. The probe diameter is 0.25 in. A range of coolant flow rates from 2.5 to 25 lb/sec was run. The ambient temperature was taken as 150°F. Other inputs are tabulated below:

<u>Thermocouple Location</u>	<u>D, in.</u>	<u>U, Btu/hr-ft²-°F</u>	<u>L, ft</u>
Reactor Outlet	8.67	3.2	7.95
Reactor Outlet	8.67	3.2	13.95
Reactor Outlet	8.67	3.2	19.95
Reactor Inlet	10	1.6	0

Axial Conduction Along the Probe

The gas duct thermocouples are 0.25-in. O.D. with a 0.035 in. sheath. The minimum immersion depth is 2 in. For a minimum expected heat transfer coefficient of 50 Btu/hr-ft²-°F (turbine outlet at 10% flow) the ml product was computed as 6.33 1/ft. The expected measurement error due to axial conduction would be as follows:

$$\Delta T_{(\text{error})} = \frac{\Delta T}{\cosh ml} \quad (\text{E-6})$$

$$\cosh 6.33 = 300$$

ΔT is the temperature difference between the gas and the duct wall temperatures. The maximum expected ΔT at the turbine outlet is 500°F. The measurement error due to axial condition would be 1.7°F. The error would be less in the reactor outlet duct since the probe heat transfer coefficient is higher. At 10% flow, $h = 100 \text{ Btu/hr-ft}^2\text{-°F}$ and the corresponding $ml = 8.94 \text{ 1/°F}$, and $\cosh 8.94 = 3666$.

FIGURE E-1. REACTOR OUTLET THERMOCOUPLE THERMAL RADIATION ERROR

FIGURE E-2. REACTOR OUTLET DUCT HEAT LOSS TO FIRST FLANGE

FIGURE E-3. REACTOR OUTLET DUCT HEAT LOSS TO SECOND FLANGE

FIGURE E-4. REACTOR OUTLET DUCT HEAT LOSS TO LAST FLANGE

FIGURE E-5. REACTOR INLET TEMPERATURE MEASUREMENT ERROR

FIGURE E-6. TURBINE OUTLET THERMOCOUPLE THERMAL RADIATION ERROR

APPENDIX F

SELECTED REDUCED DATA

APPENDIX F

SELECTED REDUCED DATA

About 100 of the datum points were reduced by the method described in Section IV-A.2. However, many of these were largely repetitious so only 22 representative points were selected and are presented here. The reduced data for the remainder of the data points of ANSOP 16625A were published earlier*.

*Internal communications: AN-AGCR-701, AN-AGCR-715.

	DATA POINT 18, 16 APRIL 1964			DATA POINT 25, 17 APRIL 1964			DATA POINT 26, 17 APRIL 1964			DATA POINT 27, 17 APRIL 1964		
	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb
1. Compressor in	80.6 (79.4)	80.4	132.41 (132.12)	24.6 (24.3)	89.0	118.15 (118.07)	40.2 (38.8)	85.0	122.12 (121.79)	65.0 (63.9)	78.0	128.45 (128.18)
2. Compressor out	288.5 (289.7)	200.3	184.95 (185.26)	91.5 (91.6)	122.5	135.18 (135.20)	174.5 (175.9)	163.5	156.19 (156.55)	276.0 (277.1)	201.5	181.80 (182.08)
3. Recuperator (HP) in	288.4 (289.6)	200.0	184.93 (185.23)	91.4 (91.5)	122.5	135.16 (135.18)	174.4 (175.8)	163.3	156.17 (156.53)	275.9 (277.0)	201.3	181.78 (182.06)
4. Recuperator (HP) out	691.7 (692.3)	195.5	287.87 (288.04)	751.0 (751.5)	121.0	303.34 (303.48)	708.2 (708.8)	161.0	292.16 (292.34)	685.3 (686.0)	196.6	286.23 (286.40)
5. Reactor in	691.7 (692.3)	195.5	287.87 (288.04)	751.0 (751.5)	121.0	303.34 (303.48)	708.2 (708.8)	161.0	292.16 (292.34)	685.3 (686.0)	196.6	286.23 (286.40)
6. Reactor out	1097.2 (1094.6)	186.0	395.73 (395.03)	1124.2 (1123.5)	117.5	403.10 (402.92)	1102.7 (1099.8)	153.0	397.25 (396.44)	1106.8 (1104.4)	186.0	398.36 (397.70)
7. Turbine in	1091.7 (1089.1)	185.5	394.23 (393.53)	1110.2 (1109.5)	117.3	399.28 (399.10)	1095.7 (1092.8)	152.6	395.34 (394.53)	1101.3 (1098.9)	185.7	396.86 (396.21)
8. Turbine out	847.6 (870.4)	83.5	328.75 (334.80)	1010.0 (1033.4)	89.6	372.12 (378.43)	909.8 (936.8)	86.0	345.27 (352.48)	845.8 (867.0)	81.0	328.28 (333.90)
9. Recuperator (LP) in	847.6 (870.4)	83.5	328.75 (334.80)	1010.0 (1033.4)	89.6	372.12 (378.43)	909.8 (936.8)	86.0	345.27 (352.48)	845.8 (867.0)	81.0	328.28 (333.90)
10. Recuperator (LP) out	385.0 (472.4)	82.9	209.34 (231.52)	271.0 (386.3)	89.5	180.54 (209.66)	320.0 (412.1)	85.8	192.91 (216.19)	374.0 (462.8)	80.4	206.55 (229.09)
11. Precooler in	382.7 (470.1)	82.0	208.76 (230.93)	268.7 (384.0)	89.5	179.96 (209.08)	317.7 (409.8)	85.5	192.32 (215.61)	371.7 (460.5)	79.5	205.97 (228.51)
12. Precooler out	80.7 (79.5)	80.4	132.44 (132.15)	24.7 (24.4)	89.0	118.17 (118.10)	40.3 (38.9)	85.0	122.15 (121.81)	65.1 (64.0)	78.0	128.48 (128.20)
Thermodynamic Power, kw		75.84			25.21			85.10			123.14	
Reactor Power to Gas, kw		1860.21 (1845.25)			776.73 (774.26)			1395.60 (1382.51)			1925.62 (1911.48)	
Recuperator Shell Loss, kw		284.05 (8.62)			182.20 (3.89)			217.34 (6.64)			296.55 (8.59)	
Compressor Flow, lb/sec		16.72			7.51			12.91			16.65	
Turbine Flow, lb/sec		16.35			7.38			12.59			16.28	
Compressor Efficiency		0.7616 (0.7517)			0.6718 (0.6676)			0.7586 (0.7417)			0.7629 (0.7536)	
Turbine Efficiency		0.8273 (0.7433)			0.9386 (0.7146)			0.8534 (0.7182)			0.8318 (0.7568)	
Recuperator Effectiveness		0.7211 (0.6933)			0.7180 (0.7008)			0.7258 (0.7004)			0.7184 (0.6931)	
Precooler Effectiveness		0.9501 (0.9636)			1.0138 (1.0101)			0.9721 (0.9824)			0.9466 (0.9607)	
Precooler Inlet Air Temp., °F		64.8			28.0			32.3			47.8	
Precooler, C _{min} /C _{max}		0.2969 (0.2971)			0.1255 (0.1254)			0.2175 (0.2175)			0.2889 (0.2891)	
T-C Set Speed, rpm		22252.0			11487.0			16500.0			22131.0	

Values in parentheses are the statistically probable values that satisfy recuperator and t-c set energy balances.

	DATA POINT 28, 17 APRIL 1964			DATA POINT 30, 20 APRIL 1964			DATA POINT 31, 17 APRIL 1964			DATA POINT 37, 18 APRIL 1964		
	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb
1. Compressor in	77.0 (76.8)	90.0	131.50 (131.44)	105.2 (104.9)	120.0	138.65 (138.59)	90.7 (90.6)	101.0	134.99 (134.96)	42.4 (41.0)	104.0	122.71 (122.34)
2. Compressor out	293.5 (293.4)	231.0	186.22 (186.20)	316.5 (316.5)	288.5	192.02 (192.02)	301.5 (301.3)	248.6	188.24 (188.19)	110.2 (111.9)	142.0	139.94 (140.34)
3. Recuperator (HP) in	293.4 (293.3)	230.8	186.19 (186.18)	316.4 (316.4)	288.3	192.00 (191.99)	301.4 (301.2)	248.4	188.21 (188.17)	110.1 (111.8)	141.8	139.91 (140.32)
4. Recuperator (HP) out	717.7 (718.3)	225.5	294.64 (294.81)	745.0 (745.6)	281.3	301.77 (301.92)	715.5 (716.1)	242.5	294.07 (294.23)	798.5 (799.2)	140.8	315.80 (315.98)
5. Reactor in	717.7 (718.3)	225.5	294.64 (294.81)	745.0 (745.6)	281.3	301.77 (301.92)	715.5 (716.1)	242.5	294.07 (294.23)	798.5 (799.2)	140.8	315.80 (315.98)
6. Reactor out	1155.1 (1154.6)	214.5	411.56 (411.42)	1151.8 (1151.3)	267.0	410.65 (410.51)	1119.0 (1118.7)	230.4	401.68 (401.60)	1167.3 (1164.0)	136.7	414.89 (413.99)
7. Turbine in	1149.6 (1149.1)	214.0	410.06 (409.91)	1147.3 (1146.8)	266.3	409.42 (409.28)	1114.0 (1113.7)	230.0	400.32 (400.24)	1153.3 (1150.0)	136.5	411.05 (410.16)
8. Turbine out	917.0 (907.1)	93.5	347.19 (344.55)	930.0 (927.4)	124.0	350.66 (349.96)	922.0 (893.5)	105.5	348.52 (340.92)	1044.3 (1074.4)	104.7	381.39 (389.55)
9. Recuperator (LP) in	917.0 (907.1)	93.5	347.19 (344.55)	930.0 (927.4)	123.9	350.66 (349.96)	922.0 (893.5)	105.5	348.52 (340.92)	1044.3 (1074.4)	104.7	381.39 (389.55)
10. Recuperator (LP) out	399.0 (487.8)	92.8	212.88 (235.44)	426.0 (503.9)	123.5	219.73 (239.56)	403.0 (483.7)	104.9	213.90 (234.39)	306.5 (401.1)	104.6	189.50 (213.42)
11. Precooler in	396.7 (485.5)	91.8	212.30 (234.86)	423.7 (501.6)	122.5	219.15 (238.97)	400.7 (481.4)	103.8	213.31 (233.80)	304.2 (398.8)	104.5	188.92 (212.84)
12. Precooler out	77.1 (76.9)	90.0	131.53 (131.47)	105.3 (105.0)	120.0	138.67 (138.61)	90.8 (90.7)	101.0	135.01 (134.98)	42.5 (41.1)	104.0	122.73 (122.37)
Thermodynamic Power, kw	185.32			122.47			109.00			21.44		
Reactor Power to Gas, kw	2273.15 (2266.99)			2640.51 (2633.36)			2298.56 (2293.55)			911.50 (901.58)		
Recuperator Shell Loss, kw	502.70 (9.72)			513.01 (12.13)			614.36 (10.68)			147.26 (4.60)		
Compressor Flow lb/sec	18.79			23.35			20.62			8.85		
Turbine Flow lb/sec	18.43			22.99			20.25			8.72		
Compressor Efficiency	0.7534 (0.7524)			0.7452 (0.7441)			0.7524 (0.7524)			0.6809 (0.6491)		
Turbine Efficiency	0.7406 (0.7703)			0.7450 (0.7523)			0.6590 (0.7548)			1.0124 (0.7047)		
Recuperator Effectiveness	0.6804 (0.6924)			0.6985 (0.7024)			0.6673 (0.7005)			0.7369 (0.7141)		
Precooler Effectiveness	0.9218 (0.9383)			0.8708 (0.8940)			0.9021 (0.9210)			0.9968 (1.0017)		
Precooler Inlet Air Temp., °F	50.0			58.0			57.2			41.7		
Precooler, C _{min} /C _{max}	0.3273 (0.3276)			0.4111 (0.4117)			0.3627 (0.3631)			0.1519 (0.1519)		
T-C Set Speed, rpm	22328.0			22159.0			22044.0			11011.0		

Values in parentheses are the statistically probable values that satisfy recuperator and t-c set energy balances.

	DATA POINT 49, 20 APRIL 1964			DATA POINT 148, 4 MAY 1964			DATA POINT 160, 6 MAY 1964			DATA POINT 168, 6 MAY 1964		
	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb
1. Compressor in	72.0 (70.0)	85.0	130.23 (129.71)	94.0 (93.1)	100.0	135.82 (135.58)	71.5 (70.5)	66.5	130.10 (129.86)	64.0 (63.0)	75.0	128.20 (127.95)
2. Compressor out	285.5 (286.8)	220.0	184.20 (184.53)	298.7 (299.1)	241.5	187.54 (187.64)	281.5 (281.9)	169.5	183.19 (183.29)	272.0 (272.4)	195.0	180.79 (180.90)
3. Recuperator (HP) in	285.4 (286.7)	219.7	184.17 (184.50)	298.6 (299.0)	241.1	187.52 (187.61)	281.4 (281.8)	169.2	183.16 (183.27)	271.9 (272.3)	194.5	180.77 (180.87)
4. Recuperator (HP) out	701.7 (702.3)	215.3	290.47 (290.63)	698.3 (699.0)	236.0	289.61 (289.77)	696.7 (697.3)	165.4	289.17 (289.34)	672.3 (673.0)	190.0	282.86 (283.02)
5. Reactor in	701.7 (702.3)	215.0	290.47 (290.63)	698.3 (699.0)	236.0	289.61 (289.77)	696.7 (697.3)	165.4	289.17 (289.34)	672.3 (673.0)	190.0	282.86 (283.02)
6. Reactor out	1130.9 (1128.2)	204.0	404.92 (404.19)	1108.2 (1107.0)	225.0	398.75 (398.41)	1127.4 (1126.1)	158.0	403.98 (403.63)	1092.3 (1091.0)	181.0	394.41 (394.06)
7. Turbine in	1125.4 (1122.7)	203.7	403.42 (402.69)	1102.7 (1101.5)	224.6	397.25 (396.91)	1121.9 (1120.6)	157.7	402.48 (402.13)	1086.8 (1085.5)	180.5	392.91 (392.56)
8. Turbine out	853.4 (878.0)	88.0	330.29 (336.82)	819.0 (881.5)	103.5	321.20 (337.75)	820.0 (883.0)	69.5	321.46 (338.13)	786.0 (853.6)	78.0	312.51 (330.33)
9. Recuperator (LP) in	853.4 (878.0)	87.9	330.29 (336.82)	819.0 (881.5)	103.5	321.20 (337.75)	820.0 (883.0)	69.5	321.46 (338.13)	786.0 (853.6)	78.0	312.51 (330.33)
10. Recuperator (LP) out	386.0 (487.3)	87.5	209.59 (230.22)	402.0 (486.5)	102.8	213.64 (235.12)	380.0 (466.8)	68.8	208.07 (230.09)	372.0 (457.4)	77.5	206.05 (227.71)
11. Precooler in	383.7 (465.0)	87.0	209.01 (229.63)	399.7 (484.2)	101.8	213.06 (234.53)	377.7 (464.5)	68.1	207.49 (229.50)	369.7 (455.1)	77.0	205.47 (227.13)
12. Precooler out	72.1 (70.1)	85.0	130.26 (129.74)	94.1 (93.2)	100.0	135.84 (135.60)	71.6 (70.6)	66.5	130.13 (129.88)	64.1 (63.1)	75.0	128.22 (127.97)
Thermodynamic Power, kw		181.80			123.91			120.48			135.42	
Reactor Power to Gas, kw		2109.12 (2092.82)			2224.27 (2214.11)			1677.25 (1669.82)			1876.90 (1868.24)	
Recuperator Shell Loss, kw		265.28 (9.21)			111.31 (10.19)			107.78 (29.22)			73.60 (8.41)	
Compressor Flow lb/sec		17.85			19.70			14.45			16.32	
Turbine Flow, lb/sec		17.47			19.32			13.85			15.95	
Compressor Efficiency		0.7650 (0.7507)			0.7604 (0.7544)			0.7632 (0.7569)			0.7784 (0.7718)	
Turbine Efficiency		0.8642 (0.7797)			0.9799 (0.7629)			0.9804 (0.7751)			0.9748 (0.7551)	
Recuperator Effectiveness		0.7329 (0.7027)			0.7681 (0.6866)			0.7710 (0.6911)			0.7789 (0.6893)	
Precooler Effectiveness		0.9324 (0.9505)			0.9076 (0.9284)			0.9665 (0.9762)			0.9511 (0.9638)	
Precooler Inlet Air Temp., °F		49.5			63.0			61.0			48.4	
Precooler, C _{min} /C _{max}		0.3106 (0.3109)			0.3490 (0.3494)			0.2554 (0.2556)			0.2835 (0.2837)	
T-C Set Speed, rpm		22197.0			21912.0			22027.0			22115.0	

Values in parentheses are the statistically probable values that satisfy recuperator and t-c set energy balances.

	DATA POINT 232, 12 MAY 1964			DATA POINT 302, 18 MAY 1964			DATA POINT 318, 19 MAY 1964			DATA POINT 324, 19 MAY 1964		
	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb
1. Compressor in	105.5 (104.6)	92.5	138.74 (138.51)	115.0 (114.1)	120.0	141.14 (140.92)	103.5 (102.5)	99.5	138.23 (137.98)	115.0 (113.8)	89.5	141.14 (140.83)
2. Compressor out	317.0 (317.3)	222.0	192.15 (192.23)	326.5 (326.9)	283.0	194.55 (194.64)	314.5 (315.0)	240.5	191.52 (191.63)	323.7 (324.4)	210.0	193.85 (194.02)
3. Recuperator (HP) in	316.9 (317.2)	221.7	192.12 (192.21)	326.4 (326.8)	282.6	194.52 (194.61)	314.4 (314.9)	240.0	191.49 (191.61)	323.6 (324.3)	209.6	193.83 (193.99)
4. Recuperator (HP) out	732.0 (732.9)	216.8	298.38 (298.62)	735.0 (735.9)	276.5	299.16 (299.38)	724.0 (724.9)	235.0	296.29 (296.52)	734.0 (734.9)	205.0	298.90 (299.13)
5. Reactor in	732.0 (732.9)	216.8	298.38 (298.62)	735.0 (735.9)	276.5	299.16 (299.38)	724.0 (724.9)	235.0	296.29 (296.52)	734.0 (734.9)	205.0	298.90 (299.13)
6. Reactor out	1143.8 (1142.7)	206.0	408.46 (408.15)	1140.2 (1139.0)	263.5	407.47 (407.16)	1130.3 (1129.1)	223.5	404.78 (404.43)	1136.2 (1134.6)	196.0	406.38 (405.94)
7. Turbine in	1138.3 (1137.2)	205.7	406.95 (406.64)	1134.7 (1133.5)	263.0	405.96 (405.65)	1124.8 (1123.6)	223.0	403.28 (402.93)	1130.7 (1129.1)	195.6	404.87 (404.43)
8. Turbine out	860.0 (911.3)	95.5	332.04 (345.67)	904.7 (911.4)	124.0	343.90 (345.70)	889.8 (898.2)	103.5	339.95 (342.17)	898.1 (909.9)	92.7	342.15 (345.30)
9. Recuperator (LP) in	860.0 (911.3)	95.5	332.04 (345.67)	904.7 (911.4)	124.0	343.90 (345.70)	889.8 (898.2)	103.5	339.95 (342.17)	898.1 (909.9)	92.7	342.15 (345.30)
10. Recuperator (LP) out	417.0 (500.9)	94.9	217.45 (238.79)	429.0 (507.5)	123.2	220.49 (240.46)	414.0 (493.1)	103.0	216.69 (236.79)	422.0 (504.4)	92.0	218.72 (239.68)
11. Precooler in	414.7 (498.6)	94.0	216.86 (238.20)	426.7 (505.2)	122.0	219.91 (239.87)	411.7 (490.8)	102.0	216.10 (236.20)	419.7 (502.1)	91.0	218.13 (239.10)
12. Precooler out	105.6 (104.7)	92.5	138.76 (138.54)	115.1 (114.2)	120.0	141.17 (140.94)	103.6 (102.6)	99.5	138.25 (138.00)	115.1 (113.9)	89.5	141.17 (140.85)
Thermodynamic Power, kw		95.95			110.51			104.30			68.22	
Reactor Power to Gas, kw		2014.65 (2004.64)			2562.62 (2549.91)			2165.22 (2153.73)			1872.92 (1861.17)	
Recuperator Shell Loss, kw		152.56 (9.15)			444.11 (11.83)			368.55 (9.98)			320.10 (8.71)	
Compressor Flow, lb/sec		18.00			23.08			19.58			17.15	
Turbine Flow, lb/sec		17.35			22.43			18.92			16.52	
Compressor Efficiency		0.7435 (0.7381)			0.7394 (0.7339)			0.7494 (0.7429)			0.7441 (0.7357)	
Turbine Efficiency		0.9519 (0.7753)			0.8051 (0.7782)			0.8113 (0.7790)			0.8207 (0.7746)	
Recuperator Effectiveness		0.7643 (0.6997)			0.7066 (0.6997)			0.7118 (0.7029)			0.7143 (0.7011)	
Precooler Effectiveness		0.9381 (0.9528)			0.8850 (0.9080)			0.9178 (0.9359)			0.9381 (0.9537)	
Precooler Inlet Air Temp., °F		85.2			74.6			76.0			95.0	
Precooler, C _{min} /C _{max}		0.3262 (0.3267)			0.4142 (0.4147)			0.3518 (0.3522)			0.3134 (0.3138)	
T-C Set Speed, rpm		22044.0			22033.0			22005.0			21978.0	

Values in parentheses are the statistically probable values that satisfy recuperator and t-c set energy balances.

	DATA POINT 174, 7 MAY 1964			DATA POINT 177, 7 MAY 1964			DATA POINT 228, 12 MAY 1964			DATA POINT 229, 12 MAY 1964		
	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb
1. Compressor in	98.0 (97.2)	101.5	136.83 (136.62)	74.0 (73.0)	79.0	130.74 (130.48)	76.0 (75.1)	83.0	131.25 (131.01)	84.0 (83.1)	80.0	133.28 (133.06)
2. Compressor out	302.5 (302.8)	243.0	188.49 (188.58)	279.5 (279.9)	200.0	182.68 (182.80)	285.2 (285.6)	210.0	184.13 (184.23)	294.0 (294.4)	200.0	186.34 (186.43)
3. Recuperator (HP) in	302.4 (302.7)	242.8	188.46 (188.55)	279.4 (279.8)	199.6	182.66 (182.77)	285.1 (285.5)	209.6	184.11 (184.20)	293.9 (294.3)	199.5	186.32 (186.41)
4. Recuperator (HP) out	690.0 (690.6)	237.5	287.44 (287.59)	666.5 (667.1)	195.0	281.34 (281.51)	682.0 (682.9)	205.0	285.36 (285.60)	696.5 (697.4)	195.0	289.13 (289.36)
5. Reactor in	690.0 (690.6)	237.5	287.44 (287.59)	666.5 (667.1)	195.0	281.34 (281.51)	682.0 (682.9)	205.0	285.36 (285.60)	696.5 (697.4)	195.0	289.13 (289.36)
6. Reactor out	1083.0 (1081.9)	226.5	391.87 (391.58)	1077.9 (1076.5)	185.0	390.48 (390.13)	1094.1 (1092.9)	194.3	394.89 (394.56)	1106.3 (1105.2)	184.0	398.22 (397.91)
7. Turbine in	1077.5 (1076.4)	226.1	390.38 (390.08)	1072.4 (1071.0)	184.5	388.99 (388.63)	1088.6 (1087.4)	194.0	393.40 (393.07)	1100.8 (1099.7)	183.6	396.72 (396.42)
8. Turbine out	806.5 (860.9)	104.5	317.90 (332.26)	775.5 (844.2)	81.5	309.76 (327.85)	800.0 (860.2)	86.0	316.19 (332.10)	817.0 (871.4)	83.2	320.67 (335.07)
9. Recuperator (LP) in	806.5 (860.9)	104.5	317.90 (332.26)	775.5 (844.2)	81.5	309.76 (327.85)	800.0 (860.2)	86.0	316.19 (332.10)	817.0 (871.4)	83.2	320.67 (335.07)
10. Recuperator (LP) out	401.0 (477.2)	103.9	213.39 (232.75)	375.0 (461.1)	81.0	206.81 (228.64)	383.0 (467.3)	85.4	208.83 (230.22)	393.0 (472.9)	82.6	211.36 (231.65)
11. Precooler in	398.7 (474.9)	103.0	212.81 (232.17)	372.7 (458.8)	80.3	206.23 (228.05)	380.7 (465.0)	84.5	208.25 (229.64)	390.7 (470.6)	81.5	210.78 (231.06)
12. Precooler out	98.1 (97.3)	101.5	136.86 (136.64)	74.1 (73.1)	79.0	130.76 (130.51)	76.1 (75.2)	83.0	131.27 (131.04)	84.1 (83.2)	80.0	133.31 (133.08)
Thermodynamic Power, kw		83.34			108.77			120.54			99.51	
Reactor Power to Gas, kw		2149.25 (2140.13)			1856.98 (1848.13)			1996.50 (1986.10)			1831.98 (1823.00)	
Recuperator Shell Loss, kw		113.87 (10.29)			72.55 (8.51)			111.28 (9.11)			109.02 (8.40)	
Compressor Flow, lb/sec		20.19			16.77			17.65			16.53	
Turbine Flow, lb/sec		19.51			16.13			17.28			15.92	
Compressor Efficiency		0.7577 (0.7524)			0.7769 (0.7701)			0.7651 (0.7592)			0.7614 (0.7560)	
Turbine Efficiency		0.9527 (0.7606)			0.9932 (0.7626)			0.9610 (0.7595)			0.9624 (0.7768)	
Recuperator Effectiveness		0.7689 (0.6949)			0.7803 (0.6863)			0.7708 (0.6915)			0.7696 (0.6984)	
Precooler Effectiveness		0.9101 (0.9290)			0.9476 (0.9614)			0.9463 (0.9597)			0.9507 (0.9626)	
Precooler Inlet Air Temp., °F		68.4			57.6			58.8			68.2	
Precooler C _{min} /C _{max}		0.3599 (0.3602)			0.2951 (0.2954)			0.3111 (0.3114)			0.2946 (0.2949)	
T-C Set Speed, rpm		21863.0			21962.0			22044.0			21929.0	

Values in parentheses are the statistically probable values that satisfy recuperator and t-c set energy balances.

Fig

	DATA POINT 337, 24 MAY 1964			DATA POINT 393, 29 MAY 1964		
	Temp., °F	Pressure, psia	Flow, Btu/lb	Temp., °F	Pressure, psia	Flow, Btu/lb
1. Compressor in	62.5 (61.5)	66.0	127.81 (127.57)	92.5 (90.6)	88.0	135.44 (134.96)
2. Compressor out	129.5 (129.7)	85.5	144.81 (144.86)	302.5 (303.7)	213.0	188.49 (188.79)
3. Recuperator (HP) in	129.4 (129.6)	85.4	144.79 (144.83)	302.4 (303.6)	212.7	188.46 (188.76)
4. Recuperator (HP) out	606.0 (607.6)	84.1	265.71 (266.13)	706.5 (707.4)	208.0	291.73 (291.96)
5. Reactor in	606.0 (607.6)	84.1	265.71 (266.13)	706.5 (707.4)	208.0	291.73 (291.96)
6. Reactor out	919.1 (917.8)	81.5	347.75 (347.41)	1111.8 (1108.6)	198.0	399.71 (398.84)
7. Turbine in	905.1 (903.8)	81.2	344.02 (343.68)	1106.3 (1103.1)	197.6	398.21 (397.34)
8. Turbine out	841.9 (844.3)	66.4	327.23 (327.89)	857.5 (879.6)	91.0	331.38 (337.24)
9. Recuperator (LP) in	841.9 (844.3)	66.4	327.23 (327.89)	857.5 (879.6)	91.0	331.38 (337.24)
10. Recuperator (LP) out	230.0 (372.2)	66.3	170.20 (206.11)	399.0 (480.4)	90.4	212.88 (233.56)
11. Precooler in	227.7 (369.9)	66.2	169.62 (205.53)	396.7 (478.1)	89.5	212.30 (232.97)
12. Precooler out	62.6 (61.6)	66.0	127.84 (127.59)	92.6 (90.7)	88.0	135.46 (134.98)
Thermodynamic Power, kw	-13.21			83.60		
Reactor Power to Gas, kw	453.43 (449.24)			1955.65 (1935.75)		
Recuperator Shell Loss, kw	199.59 (2.76)			275.87 (9.06)		
Compressor Flow, lb/sec	5.51			17.70		
Turbine Flow, lb/sec	5.24			17.17		
Compressor Efficiency	0.5991 (0.5876)			0.7418 (0.7290)		
Turbine Efficiency	0.8867 (0.8349)			0.8584 (0.7737)		
Recuperator Effectiveness	0.6690 (0.6688)			0.7279 (0.7010)		
Precooler Effectiveness	1.0261 (1.0171)			0.9476 (0.9629)		
Precooler Inlet Air Temp., °F	66.8			75.8		
Precooler, C_{min}/C_{max}	0.0982 (0.0981)			0.3180 (0.3183)		
T-C Set Speed, rpm	11071.0			22011.0		

Values in parentheses are the statistically probable values that satisfy recuperator and t-c set energy balances.

APPENDIX G

PLOTTING OUTPUT POWER AS A FUNCTION OF
COMPRESSOR INLET PRESSURE AND THE RATIO OF TURBINE
INLET TEMPERATURE TO COMPRESSOR INLET TEMPERATURE

NOTATION

h	=	enthalpy
kw	=	thermodynamic plant output, including parasitic losses
P_1	=	compressor inlet pressure (psia)
P_2	=	compressor outlet pressure (psia)
P_7	=	turbine inlet pressure (psia)
P_8	=	turbine outlet pressure (psia)
T_1	=	compressor inlet temperature ($^{\circ}R$)
T_7	=	turbine inlet temperature ($^{\circ}R$)
W	=	mass flow rate
W_1	=	compressor mass flow rate
W_7	=	turbine mass flow rate
ϕ	=	bypass flow fraction
$a_t, a_c, b_t, b_c,$	=	constants
d	=	constant
A, B, e	=	constants
N	=	turbine-compressor set speed
G	=	constant

APPENDIX G

PLOTTING OUTPUT POWER AS A FUNCTION OF
COMPRESSOR INLET PRESSURE AND THE RATIO OF TURBINE
INLET TEMPERATURE TO COMPRESSOR INLET TEMPERATURE

For pressure ratio changes of less than 10%, the referred work $\Delta h/T$ of both the turbine and compressor can be considered linear with pressure ratio, therefore:

$$\text{Total plant output, kw} = a_t W_7 T_7 \left(\frac{P_7}{P_8} + b_t \right) - a_c W_1 T_1 \left(\frac{P_2}{P_1} + b_c \right) \quad (G-1)$$

$$= a_t W_7 \frac{P_7}{P_8} T_1 \left[\frac{T_7}{T_1} - \frac{P_2}{P_1} \frac{P_8}{P_7} \frac{a_c}{a_t} (1+\beta) \right] + a_t b_t W_7 T_1 \left[\frac{T_7}{T_1} - \frac{a_c b_c}{a_t b_t} (1+\beta) \right] \quad (G-2)$$

where a_t , a_c , b_t , and b_c are constants, and β (the bypass flow fraction), is also a constant during manual control.

The turbine flow function follows a parabolic relation with pressure ratio, and around the operating point (above $\frac{P_7}{P_8} = 2.0$) it is almost independent of pressure ratio.

Hence, turbine flow can be approximated as:

$$W_7 \propto P_7 / \sqrt{T_7} \quad (G-3)$$

Pressure drops are proportional to the square of the mass flow rate divided by pressure, W^2/P , so on the high pressure side:

$$P_2 - P_7 \propto W_7^2 / (P_2 + P_7) \quad (G-4)$$

Using Equation G-3 to eliminate W_7 :

$$(P_2 - P_7)(P_2 + P_7) \propto P_7^2 / T_7 \quad (G-5)$$

Solving for P_2/P_7 :

$$\frac{P_2}{P_7} = \sqrt{1 + \frac{d}{T_7}} \quad (G-6)$$

where d is a constant.

Using experimental values of $\frac{P_2}{P_7}$ and T_7 to determine d , it is found from Equation G-6 that, for a 200°F change in T_7 , $\frac{P_2}{P_7}$ changes less than 1%. A similar situation exists for $\frac{P_8}{P_1}$, so it will be assumed that $\frac{P_2}{P_7}$ and $\frac{P_8}{P_1}$ are constants, from which it follows that:

$$\frac{P_7}{P_8} \propto \frac{P_2}{P_1} \quad (G-7)$$

and

$$P_1 \propto P_8 \quad (G-8)$$

Substituting Equations G-3, G-7, and G-8 into Equation G-2:

$$kw \propto \frac{P_1 T_1}{\sqrt{T_7}} \left(\frac{P_2}{P_1} \right)^2 \left[\frac{T_7}{T_1} - A \right] + e \frac{P_1 T_1}{\sqrt{T_7}} \frac{P_2}{P_1} \left[\frac{T_7}{T_1} - B \right] \quad (G-9)$$

where A , B , and e are constants, and the bypass valve position is assumed to be fixed.

Compressor pressure ratio, P_2/P_1 , is a function of $N/\sqrt{T_1}$ and T_7/T_1 , hence:

$$\frac{P_2}{P_1} = F \left(\frac{N}{\sqrt{T_7}}, \sqrt{\frac{T_7}{T_1}}, T_7/T_1 \right) \quad (G-10)$$

$$= G \left(\frac{N}{\sqrt{T_7}}, \frac{T_7}{T_1} \right) \quad (G-11)$$

where G is such that, for constant $\frac{T_7}{T_1}$, $\frac{P_2}{P_1}$ increases with $N/\sqrt{T_7}$.

Substituting Equations G-7 and G-8 in G-6, and re-arranging, there results:

$$\begin{aligned} kw \propto P_1 \sqrt{T_7} G^2 \left(\frac{N}{\sqrt{T_7}}, \frac{T_7}{T_1} \right) \left[1 - A \frac{T_1}{T_7} \right] \\ + e P_1 \sqrt{T_7} G \left(\frac{N}{\sqrt{T_7}}, \frac{T_7}{T_1} \right) \left[1 - B \frac{T_1}{T_7} \right] \end{aligned} \quad (G-12)$$

The $\sqrt{T_7}$ in front of both terms of Equation G-12 tends to balance the $\sqrt{T_7}$ in the function "G". Therefore, except for a small dependency of $\sqrt{T_7}$ in degrees Rankine, the plant output is a function of only P_1 and the ratio T_7/T_1 , within the limits of the approximations made above.

As a check on the above conclusions, a systems analysis digital code, "CHOP" (Ref. G-1), using experimental component characteristics and given P_1 and T_7/T_1 , calculated plant output for several combinations of T_7 and T_1 . It was found that as the quantity $(AT_1)/T_7$ approaches unity, the plant output power is drastically changed. This is probably due to the fact that the assumption in Equation G-7 becomes more significant. Figure G-1 shows the dependence of output on T_7 as a function of this quantity. If $T_7(T_1A)$ is greater than 1.08 (corresponding to design temperature ratio T_7/T_1 equals 2.8 for the ML-1), the accuracy is better than that of the measured output, so the Parametric Performance Diagram is considered valid. Equation G-12 states that plant output varies linearly with P_1 , for constant T_7/T_1 . The curves on the diagram confirm this quite closely. The test data plotted is a third

independent verification that the assumptions made in the derivation of Equation G-12 are valid.

The constant A can be determined two ways: 1) It is $(1 + \beta)$ times the ratio of the referred power ($\Delta h/T$) of the compressor to that of the turbine, and can be calculated from measured data. 2) It is also the approximate value of T_7/T_1 at which output is zero. This can be estimated quite closely by extrapolating the curves on Figure G-1. For the ML-1, both methods indicate that A is about 2.59.

It should be noted that only in the computer check of Equation G-12 did an assumption depend on the equipment used during ANSOP 16625A. Therefore, the fact that output is a function only of P_1 and T_7/T_1 is expected to apply to any Brayton cycle operating at its rated speed for $T_7/T_1 A > 1.08$.

REFERENCE

- G-1 Army Gas-Cooled Reactor Systems Program, CHOP, A Computer Program for Calculating Steady State Thermodynamic Performance, S. Luchter and M. Cobb, AGN TM-388, May 1963, Aerojet-General Nucleonics, San Ramon, California.

FIGURE G-1. PARAMETRIC PERFORMANCE DIAGRAM

APPENDIX H

REACTIVITY DATA REDUCTION METHODS

NOTATION

- k = The excess reactivity of the core in the reference cold condition, without xenon, and at the beginning of lifetime.
- k_{rods} = reactivity worth of the control rods in a just critical reactor condition
- δk_{mod} = changes in reactivity caused by changes in the moderator temperature from the reference cold condition (68°F)
- δk_{cool} = changes in reactivity contributed by changes in the coolant gas from the reference conditions of 68°F, 1 atm, 12.2 psi
- δk_{fuel} = changes in reactivity due to fuel temperature differences from the reference 68°F
- δk_{Xe} = reactivity worth of the xenon-135 fission product
- δk_{BU} = reactivity change due to fuel burnup and fission product production (except Xe-135)
- δk_{BP} = reactivity change due to burnup of burnable poison
- δk_{unk} = changes due to unknown contributions (including experimental errors)

APPENDIX HREACTIVITY DATA REDUCTION METHODS1. General Equation

The reactivity of the ML-1 system can be determined by the relationship;

$$k = k_{\text{rods}} + \delta k_{\text{mod}} + \delta k_{\text{cool}} + \delta k_{\text{fuel}} + \delta k_{\text{Xe}} + \delta k_{\text{BU}} + \delta k_{\text{BP}} + \delta k_{\text{unk}}$$

(H-1)

The algebraic signs of these terms are defined according to the changes made and are not explicit in Equation H-1. The first three of the terms on the right side (rod worths k_{rod} , moderator temperature corrections δk_{mod} , and coolant gas variations δk_{cool}) may be evaluated directly from the data collected during reactor operation and the known relationships obtained from previous experiments. The next two terms, xenon δk_{Xe} , and fuel temperature effects δk_{fuel} , may be estimated from analytical relationships but require a proportionality constant to relate the calculated result with the actual value. These constants must be evaluated from the data. The remaining terms, or rather their sum, may then be solved for. The sections below discuss each of these terms in detail and then outlines a procedure for determining the proportionality constants.

a. Control Blade Worths

The functional relationship for the reactivity worth of each shim control rod has been established to be*:

*Internal communication: Peak, R.D.; Excess Reactivity of the ML-1 Reactor; AN-AGCR-430/AN-IDOP-102; September 1963.

$$\delta k_{\text{shim}} (\% \delta k/k) = 1.482 - 0.1071(0.1\theta) - 0.03108(0.1\theta)^2 + 0.002664(0.1\theta)^3 \quad (\text{H-2})$$

and for the regulating rod*:

$$\delta k_{\text{reg}} (\% \delta k/k) = 0.442 - 0.01696(0.1\theta)^2 + 0.001257(0.1\theta)^3 \quad (\text{H-3})$$

where θ = control blade position in degrees (as read in the ML-1 control cab).

b. Moderator Worths

Reactivity worth of the moderator in the ML-1 has been experimentally determined as the following function of average moderator temperature (Ref. H-1):

$$\delta k_{\text{mod}} (\% \delta k/k) = 0.84823 - 0.01499T + 0.000037T^2 \quad (\text{H-4})$$

where T is in $^{\circ}\text{F}$ and is defined as the average of the moderator inlet and moderator outlet temperatures.

c. Coolant Gas Worths

The gas coolant contribution to the overall system reactivity depends primarily upon the gas density which in turn is a function of gas temperature and pressure. There is also a secondary effect because of the pressure expansion and contraction of the stainless steel pressure tube. The functional relationship for reactivity variations is given by**:

$$\delta k_{\text{cool}} (\% \delta k/k) = 0.17 \left(\frac{p}{T} - \frac{p_o}{T_o} \right) + 0.000066 (p - p_o) \quad (\text{H-5})$$

where;

- p = average coolant gas pressure in the core, psia
- p_o = reference gas pressure of 12.2 psia (1 atmosphere at NRTS)
- T = average coolant gas temperature in the core, $^{\circ}\text{R}$
- T_o = reference gas temperature of 528 $^{\circ}\text{R}$

* Internal communication: Peak, R.D.; Excess Reactivity of the ML-1 Reactor; AN-AGCR-430/AN-IDOP-102; September 1963.

**Internal communication: Wilcox, T.P.; Reactivity Coefficients for ML-1; AN-AGCR-238; 8 June 1962.

d. Xenon Worth

The amount of xenon-135 in the core was determined by the solution of the standard coupled iodine and xenon differential decay equations (Ref. H-2). This solution assumed that the entire reactor could be treated as a single unit with single values of flux, cross sections, and power. Furthermore, it was assumed that the power was constant over given time intervals. The resulting equation for the xenon-135 present at the end of any interval is;

$$X(t_2) = \frac{(\gamma_I + \gamma_{Xe}) \Sigma_f \phi}{(\lambda_{Xe} + \sigma_{Xe} \phi)} + \left[\frac{\lambda_I I(t_1) - \gamma_I \Sigma_f \phi}{(\lambda_{Xe} + \sigma_{Xe} \phi) - \lambda_I} \right] e^{-\lambda_I (t_2 - t_1)} \\ + \left[X(t_1) - \frac{\lambda_I I(t_1) - \gamma_I \Sigma_f \phi}{(\lambda_{Xe} + \sigma_{Xe} \phi) - \lambda_I} - \frac{(\gamma_I + \gamma_{Xe}) \Sigma_f \phi}{(\lambda_{Xe} + \sigma_{Xe} \phi) \phi} \right] e^{-(\lambda_{Xe} + \sigma_{Xe} \phi)(t_2 - t_1)} \quad (H-6)$$

where:

$X(t)$	=	xenon-135 total concentration in the reactor at time t
$I(t)$	=	iodine-135 total concentration in the reactor at time t
γ_i	=	fission yield of isotope i
λ_i	=	decay constant of isotope i
σ_i	=	absorption cross-section of isotope i
Σ_f	=	macroscopic fission cross-section for the core
ϕ	=	average thermal flux
t_1	=	time at start of interval
t_2	=	time at end of interval

In this same interval, the iodine concentration is given by:

$$I(t_2) = I(t_1) e^{-\lambda_I (t_2 - t_1)} + \frac{\gamma_I \Sigma_f \phi}{\lambda_I} (1 - e^{-\lambda_I (t_2 - t_1)}) \quad (H-7)$$

Since the entire operating history of the core is known, it is possible to use these relationships to calculate the xenon present at any time. This is done by starting from the initial reactor operation, when $I(t_1)$ and $X(t_1)$ are both zero, and calculating the value of $I(t_2)$ and $X(t_2)$ at the end of the first time interval (usually the time between the measured data points). These values are then used as the $I(t_1)$ and $X(t_1)$ in the calculation of the amounts present at the end of the second interval. And so on to the point of interest.

A value for the macroscopic thermal absorption cross section for Xe-135 equal to 3.0×10^6 barns was used. This was obtained by weighing over the fluxes obtained in a multi-thermal group calculation performed for a ML-1 cell.

The thermal flux in the xenon was obtained by the use of (Ref. H-3);

$$\phi = 6.435 \times 10^{11} P \quad (\text{H-8})$$

where P is the reactor total power in Megawatts as obtained from the Channel 5 measurements and using the proportional constants derived from the published thermodynamic data for the experiment*.

The theoretical reactivity worth of the xenon is given by:

$$\delta k_{Xe} = \frac{\delta \Sigma_a}{\Sigma_a} = \frac{W \cdot \Sigma_{Xe}}{\Sigma_a + W \Sigma_{Xe}} \quad (\text{H-9})$$

where:

$$\Sigma_{Xe} = X(t) \cdot \sigma_{Xe} = \text{macroscopic absorption cross section of the Xe-135}$$

$$\Sigma_a = \text{macroscopic absorption cross section of the core.}$$

$$W = \text{flux cell weighting factor for Xe-135 in the ML-1 system.}$$

Equation H-9 is exact. However, the cross sections are not known well enough to use this relationship directly. It was assumed, therefore, that the worth of the xenon is given by;

$$\delta k_{Xe} = a \cdot \frac{W \Sigma_{Xe}}{\Sigma_a + W \Sigma_{Xe}} = a \cdot K_{Xe} \quad (\text{H-10})$$

in which a is a constant which must be determined. In practice, it was found that a was within 10% of being equal to one. Thus indicating the validity of the approximations made.

e. Fuel Temperature

It was assumed that the reactivity effects of fuel temperature could be related directly to an average fuel pellet temperature. This temperature can be shown to be equal to:

*Internal communication: Crimmins, D.H.; Tamai, W.H.; Transmittal of Reduced and Corrected ANSOP 16625A Data; AN-AGCR-701/sd-85; 28 May 1964.

$$\bar{T}_{\text{fuel}} = b' T_g + c' P \quad (\text{H-11})$$

where; T_g is the average coolant temperature minus the reference 68°F and P is the reactor total power in Megawatts and b' and c' are constants. Then;

$$\begin{aligned} \delta k_{\text{fuel}} &\propto \bar{T}_{\text{fuel}} \\ &= \text{constant} \cdot (\bar{T}_{\text{fuel}}) \\ &= \text{constant} \cdot (b' T_g + c' P) \\ &= b T_g + c P \end{aligned} \quad (\text{H-12})$$

Again, b and c are constants which must be evaluated.

2. Solution Method

Equation H-1 may now be written:

$$(k - k_{\text{rods}} - \delta k_{\text{mod}} - \delta k_{\text{cool}}) = \delta k_{\text{fuel}} + \delta k_{\text{Xe}} + \delta k_{\text{BU}} + \delta k_{\text{BP}} + \delta k_{\text{unk}} \quad (\text{H-13})$$

The left side of this relation represents those quantities which can be readily evaluated from experimental measurements. For convenience, let

$$\Delta K' = k - k_{\text{rods}} - \delta k_{\text{mod}} - \delta k_{\text{cool}} \quad (\text{H-14})$$

Substituting the results of Equations H-10 and H-12;

$$\Delta K' = a K_{\text{Xe}} + b T_g + c P + (\delta k_{\text{BU}} + \delta k_{\text{BP}} + \delta k_{\text{unk}}) \quad (\text{H-15})$$

Now it is desired to solve for a , b , and c as well as for the three terms in brackets. To do so, it will be assumed that the burnup and poison removal will be zero over intervals of several hours. It will then be possible to solve for the three constants. At the same time, it will be necessary to assume that the unknown contribution is zero. The validity of this assumption can be checked by observing the final reduced data.

Over these short intervals of time, Equation H-15 reduces to;

$$\Delta K' = a K_{Xe} + b T_g + c P \quad (H-16)$$

The solution for a, b, and c is then done by the least squares method. This method defines a function G so that:

$$G = \min \sum_{i=1}^n (\Delta K' - a K_{Xe} - b T_g - c P)^2 \quad (H-17)$$

This implies that;

$$\frac{\partial G}{\partial a} = \frac{\partial G}{\partial b} = \frac{\partial G}{\partial c} = 0 \quad (H-18)$$

The solution to these equations was obtained algebraically and then numerically solved by a program written for the IBM-7094 computer.

REFERENCES

- H-1 Ray, J.W., et. al, ML-1-1B Critical Assembly Studies; BMI-1501, 21 February 1961
- H-2 Glasstone, S, and Edlund, M., The Elements of Nuclear Reactor Theory, D. Van Nostrand Co., New York, 1952, pp. 329-339
- H-3 Etherington, H., Nuclear Engineering Handbook, McGraw Hill, New York, 1958, pp. 1-26, 27.