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MHD HIGH PERFORMANCE DEMONSTRATION EXPERIMENT
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QUARTERLY PROGRESS REPORT

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I. INTRODUCTION

The objective of this experimental research, started late in 1973, is to demonstrate the attainment of MHD performance on a sufficiently large scale to verify that the projected efficiency of the commercial MHD concept is attainable. To perform the experiment, an existing facility at the Arnold Engineering Development Center (USAF) was re-furbished and the new systems required for performing the experiment have been designed and built. In particular, the largest systems which have required the greatest expenditure of resources are the generator channel and the 6 Tesla (T) cryogenically cooled magnet. Both of these systems are now complete and the magnet has been tested at low field strengths.

During the period between April 1, 1979, and June 30, 1979, the assembly operations on the magnet and its cryogenic cooling system were completed. The pressure testing of the generator channel was completed and the channel was installed in the magnet to facilitate final alignment of the burner. The diffuser assembly operations were completed. The magnet was retested in the warm mode, repeating the tests performed in December 1978 during which an arc-over occurred. No problems were encountered in the latest tests and the magnet was subsequently cooled to 165°K and pulsed to a peak field strength of 2.9 T. Analytic studies of the operation of the generator with supersonic flow at low magnetic fields were started and are continuing.

II. TECHNICAL PROGRESS SUMMARY

Program status at the close of the current quarter is indicated in Fig. 1. In the following, comments applicable to the progress of the subefforts are presented.

1. Facility Development

No effort was scheduled nor performed during the quarter.

2. Flow Components

The major effort scheduled was the assembly of the diffuser which was completed and the finished diffuser is shown in Fig. 2.

3. Generator Systems

Pressure testing of the generator channel was completed and the channel was installed in the magnet bore twice to check the clearances and align the burner. Installation of the power leads

3. on the channel was approximately 90 percent completed. A photograph of the completed generator channel prior to installation in the magnet bore is given in Fig. 3 which also shows the winch system employed for this purpose.

4. Magnet System

During the quarter the magnet system including its cryogenic cooling system was completed. The air conditioning system for the magnet bore purge was installed on top of the magnet at the inlet, but the duct work to the bore cannot be installed until the flow train is complete and will not be required until powered operation with the cooled magnet is started.

5. Checkout and Operation

Checkout of the magnet continued during this quarter. In April, warm mode magnet tests which duplicated the test conditions attempted in December 1978 were performed. The April test results exhibited good repeatability and indicate that the arc-over which occurred in December was spontaneous and not due to an intermittent or undetected progressive short in the magnet. In the latter part of June, the magnet coils were cooled to a mean temperature of 165°K and a single pulse, achieving a peak field strength of 2.9 T, was performed. The success of this test indicates that the cool-down can be adequately controlled and cold pulsed operation of the magnet is achievable. Results of the magnet tests are summarized in the Technical Investigations Section.

III. TECHNICAL INVESTIGATIONS

1. Summary of Magnet Test Performance

Approximately 5 min of powered operation of the magnet have been accumulated in the magnet tests performed to date and a summary of the magnet checkout tests is given in Table 1. The first four magnet tests and the power supply checkout were considered in detail in the previous quarterly report. In April, after the magnet coil repairs had been completed, the magnet was retested under the same conditions.

The measured field rise transients for the December and April tests are compared in Fig. 4. The data in Fig. 4 are from Hall probe H11 which was located on the bore center-line approximately 1 m downstream of the inlet end pole steel face. This location is approximately where the peak of the axial field distribution

1. occurs. As indicated in the figure, the repeatability of the data is good except for the low voltage case which is not explainable, but thought to be due to probe zero offset effects which have not been identified or are improperly accounted for. The correlation of the Transformer Tap 33 (2,800 v open-circuit, V_{oc}) data is excellent and indicates that the arc-over which occurred at approximately 2.5 T during the December test was a spontaneous event and not an intermittent or undetected short which progressively intensified.

The measured magnetic field profiles down the magnet bore centerline are given in Fig. 5 and compared with the predicted design distribution for 6 T scaled down to the peak of Tap 33 data. The slope of the field distribution appears to increase slightly with increasing peak field strength at these low fields but is in good agreement with scaled prediction. At higher fields, the influence of the steel is minimized due to saturation and it would not be expected that the slope of field distribution would change from the results given in the figure. The measured field strengths for off-axis probe locations (H1, H3, H5, H7, H10, and H12) are also given in the figure. These positions are symmetrically located about the bore centerline in a horizontal plane and spaced from the centerline a distance approximately equal to the channel inside half-width for the indicated axial locations. Ideally, the paired off-axis field measurements should be equal and greater than the centerline value due to their closer proximity to the coils. The data of Fig. 5 indicate mild transverse gradients and asymmetries which do not appear to be severe nor significant insofar as generator channel operation is concerned.

In the last week of June, the magnet coils were cooled to 165°K and the magnet was pulsed using the lowest power supply tap setting at 1,400 v with no current limiting reactance in the primary supply. As a consequence of the lowered resistance of the cooled coils, the highest field and current of 2.9 T and 5,437 amp, respectively, were obtained. A comparison of the current rise transients in the warm and cooled coils with the same applied voltage is given in Fig. 6. As indicated in the figure, the time constant, defined as the time to achieve 63 percent of the peak current, approximately doubled in the case of cooled coil which had a nominal resistance 45 percent of that of the warm coil. This is analogous to a simple inductance-resistance circuit and illustrates the fact that the presence of the shorted force containment structure has little effect on the gross transient characteristics of the magnet.

2. Initial Magnet Strain Gage Results

Approximately 140 strain gages are attached to the magnet force containment structure to monitor the structural load distribution

2. during the magnet operation. These gages are attached to selected members expected to have the highest stresses. The gages are mounted on structural members located primarily on the magnet outlet end and along the left (East) side. Members gaged include: the longitudinal tension members, the outlet faceplate, the upper outlet tongue, the upper transverse tension member number 10, the upper outlet saddle cap and the left (East) side saddle plate.

Of these gages, 45 were selected to be monitored during the warm magnet runs. The selections were made to aid in defining the load path distribution. Ten gages were connected to the Preston low level data acquisition system and each gage was monitored at the rate of 100 samples per second and recorded on magnetic tape. The remaining 35 gages were connected to a Sun Systems, Adacus strain gage data logger and each gage was monitored once every 1.5 sec.

The strain gages are chosen to have self-temperature compensation and negligible magnetoresistive effects over the planned operating range. In addition, the strain gages are mounted in a full bridge configuration with one active gage attached to the structure and three "dummy" gages attached to a nearby piece of unstressed aluminum. This configuration gives a further cancellation of temperature and steady-state magnetic effects. However, the strain gages are vulnerable to voltages induced by changes in magnetic flux linkage with the gage circuit. For this reason, the strain gages are expected to be useable only when the magnet approaches a quasi steady-state condition near the peak of the current pulse.

Strain gage data have been compiled for the first four magnet tests in Table 1. Of these four, only tests -002 and -003, which have peak field strengths of 1.78 and 2.26 T, respectively, were of sufficient duration and field to produce meaningful strain data. In addition, the full-scale range of the strain gage data logger is 5,000 $\mu\epsilon$ ($\mu\epsilon$ = microstrain = 10^{-6} cm/cm) and it is considered that the measured strains are meaningful only when the absolute measured value exceeds 50 $\mu\epsilon$ or 1 percent of full scale. Imposing this lower bound, the strains measured on all of the structural members except the longitudinal tension members were insignificant and will not be considered reportable.

The locations of all the strain gages mounted on the longitudinal tension members are indicated in Fig. 7. All gages on this member are oriented to measure longitudinal strains only. The resulting strains measured at the selected locations during the two magnet tests of interest are given in Table 2. As indicated, the most significant strains were compressive and located on the fingers of the LTM where they pass through the faceplate. An explanation for the observed compressive strains is that the load transference

2. from the faceplate to the fingers can be eccentric. The eccentricity would impose a moment in addition to a tensile load on the fingers. For light loadings, the compressive strain of the moment on the strain gage side opposite the slot is dominant and observed. It would be expected that at increased loading these strains would eventually become tensile. The strains measured in the remainder of the LTM were small and tensile as would be expected.

Measurements from one of the LTM strain gages monitored with the data acquisition system are given in Fig. 8. Each of the data points indicated in Fig. 8 is actually a centered average of approximately twenty separate sample points. Even after averaging, considerable scatter remains in the data for the period in which the current and field are rising in the magnet. The source of this noise is not known; however, the data indicate that the noise diminishes at the quasi-steady current conditions at the pulse peak and is absent during the field collapse after power supply shutoff. This suggests that it is sourced in the power supply to the magnet.

An example of the strain gage data as recorded with the data logger is given in Fig. 9. The lower scan rate of the data logger does not allow meaningful averaging of the data and, as a consequence, scatter is evident. However, the noise diminishes near the pulse peak for this system as well and meaningful data can be obtained. Some improvement in the data logger readings can be expected when the magnet is cooled for high field operation. In this instance, the time constant of magnet will be increased which, in turn, will increase the number of data points during the field rise which may permit a better definition of the strain transients.

To assess the magnetic effects on the strain gages, a dummy gage was placed in the magnet bore and monitored with the data acquisition system. The results, with averaging as previously described, are given in Fig. 10. The results indicate that the noise is not load dependent and correlates quite well with time of occurrence and magnitude with the previous results of Fig. 8. The dummy gage data also exhibit a functional dependence on the temporal change of the field (dB/dt) but no significant dependence on the field strength itself (B). The data from the dummy gage also exhibits step jumps when the coil current is turned on and off. This behavior is less pronounced in the active gage data of Fig. 8 and due to the slow scan rate of the data logger is absent in the trace in Fig. 9. In general, it can be expected that all of the strain gages will exhibit anomalous readings when the coil current is turned on and off, but that the magnitude of the readings will be less than 50 $\mu\epsilon$.

3. Magnet Cooldown

Cooldown of the HPDE magnet to an overall mean temperature of 165°K was accomplished during this period. The cooldown process consisted of three phases of operation. First, all residual moisture was driven from the coils and force containment structure using a dry nitrogen purge. Secondly, a preliminary test was conducted to check out operation of the LN₂ feed system. Finally, the magnet was cooled by flowing LN₂ through the coils.

A continuous dry nitrogen flow of approximately 70 l/sec was used to dry the magnet coils and the enclosing structure. This purging process brought the moisture content of the coils from a level greater than 1,000 ppm (parts per million) to less than 155 ppm in three days. Once drying was completed, a nominal purge flow was maintained until the cooldown process commenced.

Following the drying process, a preliminary test was conducted to check out the LN₂ feed system and to identify any operational problems which might be encountered during actual magnet cooldown. In this test, all LN₂ transfer lines which extend from the LN₂ supply dewar to the magnet manifold control valves were cooled to LN₂ temperature. This cooling was completed in less than three hours. After transfer line cooldown had been achieved, a LN₂ flow through the four LN₂ flowmeters and into the magnet manifold was initiated for about 15 min. In the process, the upper west coil flowmeter bearing failed from the passing two-phase flow. All flowmeter readouts showed large fluctuations and, as a result, it was evident that control of flow rate to the different coil sections could best be achieved by regulating the inlet supply pressure upstream of the manifold control valves and controlling valve position. In this fashion, the flow to any of the four coil sections could be estimated fairly well from the most stable flowmeter readout. This procedure was used to control flow rate during the subsequent magnet cooldown in which only one flowmeter survived the entire test.

Actual cooldown of the magnet began on June 28 and encompassed two days of operation. The objective of this first magnet cooling was to achieve a mean coil temperature of 160°K and to pulse the magnet at this reduced temperature. Cooling was provided by flowing LN₂ through the magnet coils only with adjacent members of the FCS cooled by heat conduction and natural convection within the structure. Instrumentation used to continuously monitor the cooldown process consisted of 152 thermocouples and 75 strain gages located throughout the FCS. Measurements of coil, half-coil, and coil pancake electrical resistance were periodically taken to provide tracking of the coil mean

3. temperatures. Table 3 lists the coil pancake resistances before and after cooldown with the calculated temperatures after cooldown. A time-history of the mean coil temperature variation during cooldown is presented in Fig. 11.

Cooldown began by flowing LN_2 at approximately 600 ℓ/hr uniformly to each of the four coil manifolds. The first four hours of operation were primarily spent in cooling the magnet inlet manifolding with no significant reduction in the mean coil temperature observed. After this initial period, the coil temperature began to decrease steadily at a rate of $11^\circ\text{K}/\text{hr}$. This rate was maintained throughout the first day of cooldown. Flow was stopped overnight for 8 hr to allow thermal soak of the FCS and relaxation down the coils. During shutdown, a rise of approximately 12°K in mean coil temperature was observed. The second day of cooldown began using the previous day's LN_2 flow rate for the first 4 hr, after which the flow rate was cut to approximately one-half this value (300 $\ell/\text{hr}/\text{manifold}$). At this reduced rate, the coil cooled at approximately $3.0^\circ\text{K}/\text{hr}$. The thermal behavior of the coil at the two mean flow rates used during cooldown agrees well with the theoretical predictions. At the 600 ℓ/hr flow the agreement with theory was within 1 percent.

During the last hour of cooldown, flow of LN_2 to the lower half of the coil was increased. This flow regulation was used to eliminate a thermal gradient which developed between the upper and lower halves of the coil. The upper half cooled slightly faster than the lower; as much as 38°K difference in their mean temperatures was observed. By distributing the LN_2 flow to allow more cooling in the lower coil, the difference was reduced to 6°K by the finish of cooldown.

Pancake-to-pancake mean temperatures were also tracked during cooldown. Typical development of these gradients is illustrated in Fig. 12. As indicated, the maximum temperature difference between any adjacent pancakes was maintained to less than 19°K . Longitudinal gradients in the coils were inferred by temperature readouts of thermocouples imbedded in the 1/2-in. aluminum plate which separates the upper and lower coil halves. These longitudinal gradients were rather large, as anticipated, since the coils are cooled from one end of the structure. As much as 128°K temperature difference was observed from end-to-end readings of the coil separator plate thermocouples.

Cooldown of the FCS structure lagged that of the magnet coils since no direct LN_2 cooling of the structure was used. Thermal gradients within the whole structure were, for the most part, well behaved. No large stresses were evidenced in any of the members based on the strain gage readouts. The most severe thermal gradient existed from top to bottom in the end box containing the LN_2 inlet manifolding. As much as 70°K was observed across the collar and faceplate of this

3. end box. This gradient probably resulted from a combination of natural convection within the end box and the effect of gravity on LN₂ flow in the crossover of the lower half-coil. No attempt was made to damp out this gradient since the measured stress levels were within acceptable limits.

Upon completion of cooldown, the magnet was electrically pulsed as discussed in Section 1. The mean coil temperature was measured after the pulse and showed a temperature rise of approximately 10°K. Monitoring of the coil temperature and FCS thermocouples continued for several days following the cooldown as the magnet and structure warmed back to room temperature conditions. Preliminary estimates indicate a time constant (time required for initial gradient to relax to 63 percent of its initial value) required for structure thermal soak of approximately 51 hr and an effective steady-state heat loss to the surroundings of approximately 7.0 kw (based on a mean structural temperature of 200°K).

4. Magnet Development Tests

During the investigation subsequent to the coil arc-over, the insulative integrity of the spacer plates in the coil saddle crossovers was questioned. The coil spacer plates are comprised of aluminum plates with Delrin® or G-10 sheets bonded to the front and back faces. These plates are placed between adjacent coil pancakes in the saddle crossover regions to provide a load path through the coils with clearance allowances for the coolant tubes and in the case of the exit saddle, to compensate for coil manufacturing tolerances. The point of concern was that since the edges of these plates were uninsulated and in contact with the FCS, they would be at the same potential as the structure and thus be a possible shorting path in the event that the Delrin or G-10 and coil insulation break down with applied voltage under load.

A test setup as depicted in Fig. 13 was devised. A small aluminum block faced with Delrin of the same thickness as used in coil spacer plates was placed on a two-piece conductor assembly insulated identically to the installed coil pancake insulation. The assembly was subjected to compressive loading through G-10 laminated fiberglass blocks for electrical isolation. An electric potential was impressed between the simulated spacer piece and the coil conductor pieces. The following results were obtained:

1. No arcing or insulation breakdown was observed with 9,000 v (three times the design working value) applied up to and including a maximum bearing pressure of 10,000 psi.
2. With no load applied, no arcing or insulation breakdown was observed up to and including 20,000 v applied potential.

4. 3. With no load applied and all of the insulation removed from the copper conductor lengths, the applied voltage was gradually increased until arcing occurred at 4,100 v. The observed arcs occurred in the air gap at the edge of the Delrin rather than through the Delrin.

It was concluded that the Delrin 500 used in the saddle fillers exhibited no deleterious properties under compressive load and when used in conjunction with the coil insulation, exhibited excellent electrical insulative properties.

A second point of concern which arose in the course of the investigation of the magnet arc-over was the fatigue life of the coolant tubes and their electrical insulation, particularly at the coil exit where differential thermal contractions of the structure and coils are accommodated. Each conductor of the HPDE magnet coil is supplied with coolant through a copper tube brazed into a copper water fitting installed on the conductor. The copper coolant tubes are enveloped in a fiberglass NEMA Grade G-10 "tube holder cover" with 0.13-cm thick walls and a G-10 spacer bar between adjacent tubes to transmit compressive forces in the saddle regions and prevent crushing of the tubes. The coolant tubes are protected with a 2.54-cm thick G-10 insulating bushing at the inside and outside edges of the slotted penetrations in the aluminum sideplates of the FCS. Each coolant tube is sleeved with heat shrinkable tubing. The point of concern was that when the coils are cooled, they will shrink relative to the FCS. The coolant tubes, by being rigidly clamped at the coils and at the structure penetration, are subjected to a bending moment and possible fatigue failure.

To experimentally investigate the hypothesis, a test jig as depicted in Fig. 14 was devised. The test setup included a piece of conductor with a water-block fitting and a length of coolant tube attached. The coolant tube was surrounded with G-10 strips to simulate the tube holder cover and the assembly was clamped to an I beam which, in turn, was placed on the bed of a testing machine. The ram of the testing machine was fitted with an adapter to simulate the sideplate bushings which held the free end of the coolant tube. The free end of the coolant tube was fitted with a flexible line identical to those used in the magnet assembly to provide coolant line electrical isolation. Liquid nitrogen was fed through the coolant line to simulate cold magnet operating conditions. With this setup, three tests were performed and are described in the following.

Test No. 1 - With the simulated spacer bars clamped around the coolant tube and a length of polyolefin shrinkable tubing placed over the cooling tube, LN₂ was passed through the coolant tube until liquid was discharged at the outlet end. Then the coolant

4. tube was cycled 300 times with a 2.54-cm upward deflection and return to neutral with LN₂ temperature being maintained throughout. Posttest inspection revealed no evidence of abrasion or cracking of the shrinkable tubing; however, the copper coolant tube was kinked or crimped where it entered the simulated tube holder cover. Subsequent Zyglo® test of the afflicted area revealed no cracks.

Test No. 2 - This test was a repetition of the first test except that a short length of Korvex FEP 1.3x heat shrinkable tubing was installed in the area of the machine ram fixture and the remainder of the coolant tube was left bare. The tube was initially cycled 150 times at room temperature, followed by cooling with LN₂ and cycling an additional 300 times. Crimping of the copper tube similar to that of the first test occurred during the warm cycling but did not appear to progress or worsen during the subsequent cold cycling. The posttest Zyglo examination did not reveal any cracks in the afflicted area.

Test No. 3 - For this test, Korvex FEP 1.3x tubing was installed over the whole length of the coolant tube with a second layer of polyolefin tubing in the section within the area of the ram fixture. The simulated tube holder cover pieces were not used in this test which was intended to test the water block fitting. The fixture was cycled 150 times at room temperature and an additional 300 times when cooled with LN₂. The sample was visually inspected and then Zygloed with no adverse effects noted.

The coolant tube fatigue tests produced no failures of either the copper tubes or the soldered joints at the tube-to-water block and water block-to-conductor junctions. Although the copper tube kinked at the tube holder cover in Tests 1 and 2, there was no strain failure. The flexible hose used in all of the tests was cycled in excess of 1,000 times and successfully pressure tested to 400 psi at the conclusion of the tests.

A third area of concern which arose as a consequence of the investigation following the arc-over was the structural integrity of bus bar assembly and the power lead connection to the magnet. This assembly is shown in Fig. 15. The bus bars are rigidly supported on their upper end by attachment to the building framework and attached at their lower end to the magnet power leads. The bus bar is potentially loaded by three processes. First, contraction of the magnet power leads upon cooling imposes a downward tensile load. Secondly, any motion due to contraction of the FCS imposes a horizontal loading with attendant bending moments. Thirdly, during powered operation of the magnet, the interactive forces of the coil current and magnetic fields are imposed on the bus bar and power leads.

4. To investigate the integrity of the bus bars, a structural analysis employing a three-dimensional space frame modeling of the bus bars and magnet coil leads was performed. As a consequence of this analysis, additional strutwork between the bus bar paths was incorporated into the completed magnet structure. One such strut is indicated in Fig. 15.

5. Generator Channel Analytical Studies

As a result of several meetings with DOE personnel and their designated consultants, it has been concluded that the testing of the HPDE system at AEDC should proceed not to achieve the performance goals as quickly as possible, but rather to progress toward these goals in steps of incremental stress on the hardware, particularly the magnet. In this regard, the possibility of supersonic channel operation in the Faraday and two-terminal diagonal modes should not be discounted. To implement such a test plan, analytical channel performance calculations have been initiated to investigate operation with supersonic flow in the Faraday configuration at low magnetic field strengths up to 4 T.

For the analysis, a computer code which combines a chemical thermochemical equilibrium calculation with a one-dimensional MHD channel analysis is being used to obtain candidate operating conditions for the HPDE. The chemistry calculation determines equilibrium products for toluene burned with a specified N_2/O_2 ratio together with a mixture of methanol and potassium hydroxide, which properly accounts for the seed solubility. At present, the generator calculation is being made for supersonic operation with resistive loading, mass flow, and magnetic field strength specified.

From previous experience, a constant RL (RL = load resistance times electrode pitch) of 0.8 ohm-m is taken to be a nominal operating condition. For a mass flow of 55 kg/sec and a peak B-field of 4 T, N_2/O_2 ratios of 0.75, 1.0, and 1.25 have been considered. The N_2/O_2 ratios of 0.75 result in the highest performance and the lowest Hall fields but a more moderate ratio of 1.00 has been chosen as a reasonable compromise with regard to the channel heating load. The nominal loading was adjusted at the exit end of the channel in order to reduce the Hall field and thus the possibility of arcing at the channel exit. The chosen loading distribution consists of an RL equal to 0.8 ohm-m from 0.0 to 6.5 m with a linear variation from 0.8 ohm-m to 10 ohm-m over the last half-meter.

With the selected loading distribution, the mass flow and B-field were reduced to 45 kg/sec and 2 T, respectively. At these conditions the chosen load appears to be a near optimum choice as far as Hall field and exit stagnation pressure are concerned. However, with the light loading, the Mach number at the exit approaches 2.2.

5. If a double normal shock loss and diffuser recovery coefficient of 0.5 are assumed, the available stagnation pressure at the exit is marginal. In actuality, a normal shock will most likely situate itself in the last one-half meter of the channel. With the above-mentioned reduction in loading in this area, the Hall field is reduced along with the possibility of arcing.

Table 4 is a summary channel performance at low and moderate magnetic field strengths with the resistive loads of the load bank maintained constant. While reasonable performance at the 4T field is obtained, under the 2 T conditions, poor performance and utilization of the driving combustion pressure are evident. However, it would appear that respectable power levels should be rather easily obtained even at the low fields which can be achieved without cooling the magnet.

Supersonic channel flow has the advantage that the combustion chamber is isolated from flow perturbations which occur in the channel. Similarly, the channel flow is isolated from perturbations introduced in the diffuser. However, the diffuser for the HPDE was designed to be optimizable for subsonic operation and as such its supersonic recovery capability is limited. Fortunately, it can be expected that reasonable recovery can be achieved for flow Mach numbers less than 2.0. When operated with a supersonic inlet flow, the total pressure losses in the diffuser can be considered to be due to a normal shock loss in addition to a subsonic diffusion loss downstream of the shock. With increasing entrance Mach number, the shock losses will increase while the subsonic diffusion losses will diminish in accordance with the decrease in Mach number downstream of the shock, as shown in Fig. 16. The sum of these two loss mechanisms has a local minimum in the supersonic range at approximately $M = 1.5$.

IV. DISCUSSION

WORK PLANNED FOR THE NEXT QUARTER

The following work on the major subefforts is planned for the next quarter.

1. Facility Development

No effort is scheduled.

2. Flow Components

The flow train will be placed in an operational status.

3. Generator Systems

The generator channel and load bank will be placed in an operational status in the segmented Faraday configuration.

4. Magnet Systems

The magnet will be allowed to warm to room temperature and be operational for warm mode operation.

5. Shakedown and Operation

Unseeded burner operation with and without concurrent magnet operation will be performed to check out the burner and cure the channel refractory. A power producing run on the channel is anticipated to be performed during this quarter. Analytical generator performance studies and compilation of magnet cold test results will continue.

Table 1

Summary of Magnet Checkout Tests

Test No.	Date	Peak Voltage, volts	Peak Current, amp	Peak Field, Tesla	Duration, sec	Comments
004-001	12/12/78	1,400	2,403	1.53	20.9	Short duration check.
004-002	12/12/78	1,400	2,801	1.78	49.5	Warm test.
004-003	12/20/78	2,100	4,066	2.26	46.9	Warm test.
004-004	12/20/78	2,800	4,250	2.32	15.6	Terminated by arc-over.
004-005	3/29/79	3,200	13,800	-	-	Power supply check.
004-006	4/5/79	1,400	2,813	1.74	43.7	Warm test
004-007	4/5/79	2,100	4,010	2.28	42.0	Warm test
004-008	4/5/79	2,800	4,917	2.64	30.8	Warm test
005-001	6/28/79	-	-	-	2 days	Cooldown test only.
005-002	6/29/79	1,400	5,437	2.91	52.1	Cooled test.

Table 2

Peak Measured Strains

Member	Gage Location	Gage Designation	Measured Strain, $\mu\epsilon$	
			Run 2, 1.78 T	Run 3, 2.26 T
Left Longitudinal Tension Member	Finger	LTML-1	-135	-290
	Finger	-2	-150	-300
	Finger	-3	-	-420
	Finger	-6	-195	-300
	Finger	-10	-225	-370
	Downstream of Key	4	<50	-75
	Downstream of Key	8	<50	<50
	Upstream of Key	12	+240	-
	Upstream of Key	13	+225	+310
	Midspan, outside	14	<50	<50
	Midspan, inside	16	60	+90
	Midspan, outside	15	+75	+140
	Midspan, inside	17	<50	<50

Note: (+) represents extension of the gage and (-) represents compression of the gage.

Table 3

Coil Pancake Resistance Measurements

Coil Pancake No.*	Resistance at Room Temperature	Resistance After Cooling	Implied Cold Temperature, °K
1U	0.0235	0.0128	183
2U	0.0240	0.0113	164
3U	0.0246	0.0103	151
4U	0.0254	0.0203 [†]	144
5U	0.0260		
6U	0.0266	0.0115	154
7U	0.0273	0.0122	158
8U	0.0279	0.0130	163
9U	0.0285	0.0139	168
1L	0.0235	0.0127	182
2L	0.0240	0.0115	166
3L	0.0246	0.0106	154
4L	0.0253	0.0105	150
5L	0.0259	0.0104	146
6L	0.0265	0.0104	144
7L	0.0272	0.0101	139
8L	0.0278	0.0109	144
9L	0.0284	0.0100	134
Sum	0.4670	0.2024	154

*U = Upper, L = Lower

[†] Pancakes 4U and 5U could only be read together due to a faulty connection.

Table 4

Summary of Channel Analytical Studies with Stoichiometric Combustion

$\dot{m}$ kg/s	N ₂ /O ₂	B Tesla	E _x v/m	E _y v/m	V _x volt	σ Entrance mho/m	Power MW	Enthalpy Extraction %	Turbine Efficiency %	Exit Stagnation Pressure atm	M _{Exit}
45	0.75	2	-1,000	2,500	3,690	13.6	19.8	7.4	64	2.61	2.14
45	1.0	2	-1,500	2,300	5,190	10.5	16.8	7.0	57	2.64	2.16
45*	1.0	2	-1,450	2,390	4,860	11.2	18	6.8	76	2.7	2.14
45	1.25	2	-2,600	2,150	7,820	7.6	13.4	6.2	49	2.71	2.19
55	0.75	4	-1,400	4,200	6,425	12.8	50.0	15.0	74	1.57	1.44
55	1.0	4	-1,950	4,000	9,270	9.6	44.4	15.0	70	1.58	1.51
55	1.25	4	-2,800	3,600	12,960	7.2	38	14.1	64	1.59	1.54

*Off Stoichiometric: $(O/F)/(O/F_{stoic}) = 0.90$

Δ SUBEFFORT BEGINNING
 ▽ SUBEFFORT COMPLETION
 SOLID BAR INDICATES WORK ACCOMPLISHED
 BROKEN BAR INDICATES SLIPPAGE AND RESCHEDULING OF ACTIVITIES

HPDE PROGRAM SCHEDULE

REVISED 7-7-78

Figure 1 HPDE Program Schedule

Figure 2. Photograph of HPDE Variable Wall Angle Subsonic Diffuser

Figure 3. Generator Channel Installation on its Cart Prepared for Placement in the Magnet

Figure 4. Measured Field Rise Characteristics of HPDE Magnet for Probe Position H11, 1 m from Inlet Pole Face

Figure 5. Magnetic Field Strength Distribution along Magnet Bore

Figure 6. Comparison Coil Current Pulses for Warm and Cold Operation with Power Supply Transformer Tap Setting 1 (Open Circuit Voltage = 1,400 v)

Gage	Z, in.	Y, in.
LTM-L- 1	12.25	0.50
2	12.25	5.03
3	12.22	9.46
4	17.87	10.00
5	12.22	15.54
6	12.22	20.00
7	12.19	24.45
8	17.97	30.01
9	12.22	30.55
10	12.22	35.01
12	42.59	10.03
13	42.60	30.03
14	140.35	10.00
15	140.35	30.06
16	139.57	10.02
17	139.63	30.06
19	237.89	30.04
22	351.90	9.93
23	351.93	30.09

NOTE:

*Inside surface

Gages on outside surface unless noted.

Gages oriented to measure strains in flow direction.

Figure 7. Strain Gage Location on the Left Longitudinal Tension Member

Figure 8. Strain Measured on the Preston Low Level Data Acquisition System

Figure 9. Strain Measured on the Sun Systems, Adacus Data Logger

Figure 10. Dummy Strain Gage in the Magnet Bore

Figure 11. HPDE Magnet Cooldown Results (6-28-79 - 6-29-79)

Figure 12. HPDE Magnet Coil Pancake Mean Temperatures during Cooldown

Figure 13. Schematic of Test Setup used to Investigate Insulation Properties of Coil Spacer Plates under Compressive Load

Figure 14. Diagram of Test Setup for Investigating Effect of Fatigue Loading on Coil Coolant Tubes

Figure 15. Photograph of Magnet Bus Bar Terminations Showing Added Brace

Figure 16. Expected Stagnation Pressure Losses with a Subsonic Diffuser Operating with a Supersonic Inlet Flow