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MHD PERFORMANCE DEMONSTRATION EXPERIMENT

Report for the Period October 1, 1980—September 30, 1981

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**REPORT ON THE
MHD PERFORMANCE
DEMONSTRATION EXPERIMENT
OCTOBER 1, 1980 TO SEPTEMBER 30, 1981**

**G. L. Whitehead, L. S. Christensen, E. J. Felderman
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CONTENTS

	<u>Page</u>
ABSTRACT	1
I. INTRODUCTION	2
II. FACILITY DESCRIPTION AND OPERATION	
2.1 Facility Description	3
2.2 Facility Operation	12
III. FACILITY REPAIR AND IMPROVEMENT	
3.1 Magnet Bore Liner	19
3.2 Generator Channel	19
3.3 Patch Panel	20
3.4 Combustor System	20
3.5 Load Bank	23
3.6 Diffuser and Breech	23
3.7 Modified Control Panel	23
3.8 Instrumentation Improvement	24
3.9 Channel Flange Loads and Preload System	24
3.10 Miscellaneous System Changes	26
3.11 System Safety Hazard Analysis	27
IV. MAGNET PERFORMANCE	
4.1 Quasi-Steady-State Magnet Performance	29
4.2 Stress Analysis	33
V. FARADAY GENERATOR PERFORMANCE CHARACTERISTICS	
5.1 Power Production	50
5.2 Transverse Voltage Characteristics	58
VI. SUMMARY	74
REFERENCES	75
NOMENCLATURE	76

ILLUSTRATIONS

Figure

1. Elevation View and Coordinate Convention for the HPDE Flow Train and Magnet	4
2. HPDE Magnet	5
3. Generator Channel Resting on its Assembly Cart Prior to Insertion into the Magnet	7
4. Elevation of HPDE Flow Train	9
5. Installed Diffuser and Upstream Portion of the Breech . .	10

<u>Figure</u>		<u>Page</u>
6.	Liquid Rheostat Load Bank	11
7.	HPDE Data Reduction System	13
8.	System Control Center	15
9.	HPDE Selected Test Data Illustrating a Typical Run Sequence	16
10.	Variation of Selected Key Parameters during HPDE Run MI-007-006	18
11.	Variation of the Diffuser Voltage to Ground with Time . .	22
12.	Preload System	25
13.	Location of the Hall Probes on the Floor of the Magnet Bore	31
14.	Steady-State Magnet Characteristics	32
15.	Axial Field Distribution in Magnet	34
16.	Location of Strain Gage Instrumentation on HPDE Magnet Force Containment Structure	35
17.	Strain Gage Locations on the Left Longitudinal Tension Member	36
18.	Strain Gage Locations on the Outlet Faceplate	37
19.	Strain Gage Locations on the Upper Outlet Tongue	38
20.	Strain Gage Locations on Transverse Tension Member, Upper 10	39
21.	Strain Gage Locations on the Upper Outlet Saddle Cap . .	40
22.	Strain Gage Locations on Left Outlet Sideplate	41
23.	Axial Strain along the Surface of the Left and Right Longitudinal Tension Members	43
24.	Strain Measured on the Outlet Faceplate	44
25.	Strain Measured on the Upper Outlet Tongue	45
26.	Strain Measured on the Upper Transverse Tension Member .	46

<u>Figure</u>	<u>Page</u>
27. Strain Measured on the Upper Outlet Saddle Cap	47
28. Strain Measured on the Left Sideplate	49
29. Resistive Loading Distribution	51
30. Development of Cathode and Anode Voltage along the Channel Walls	
a. $B = 2.1\text{ T}$	52
b. $B = 2.8\text{ T}$	53
c. $B = 3.1\text{ T}$	54
31. Development of Faraday Voltage, Current, and Power along the Channel, $B = 2.1, 2.8$, and 3.1 T	56
32. Enthalpy Extraction for Faraday Mode Tests	57
33. Maximum Expected Enthalpy Extraction	59
34. Static Pressure Distribution along Channel and Diffuser Walls	60
35. Plan View of Channel Showing Location and Designation of Pegwall Voltage Measurement Points in Top Insulating Wall	62
36. Voltage Distribution across the Channel 1.447 m Downstream of Channel Inlet	63
37. Voltage Distribution across the Channel 2.79 m Downstream of Channel Inlet	65
38. Voltage Distribution across the Channel 4.63 m Downstream of Channel Inlet	67
39. Voltage Distribution across the Channel 6.279 m Downstream of Channel Inlet	69
40. Variation of the Measured Voltage Drop on the Anode and Cathode Walls along the Length of the Channel	71
41. Variation of Total Voltage Drop with Time (Surface Temperature)	72
42. Surface Temperature Distribution along HPDE Electrode Wall	73

TABLES

1. List of Parameters Monitored by Annunciator System	14
2. Magnet Run Summary	30
3. Parameter Summary of the First Three Powered Runs (Pyrolytic Graphite Caps)	50

APPENDIX

A. DEFINITION OF PERFORMANCE PARAMETERS	A-1
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ABSTRACT

The Arnold Engineering Development Center (AEDC) has been under contract with the Department of Energy (DOE) since December 1973 to conduct a magnetohydrodynamic (MHD) High Performance Demonstration Experiment (HPDE). The objective of this experimental research is to demonstrate the attainment of MHD performance on a sufficiently large scale to verify that projected commercial MHD objectives are possible. This report describes the testing of the system under power-producing conditions during the period from October 1, 1980, to September 30, 1981.

Experimental results have been obtained with the channel configured in the Faraday mode. Test conditions were selected to produce low supersonic velocity along the entire channel length. Tests have been conducted at magnetic fields up to 4.1 Tesla (T) (70 percent of design). Up to 30.5 MW of power has been produced to date (60 percent of design) for an enthalpy extraction of approximately 11 percent.

The high Hall voltage transient, observed during the previous series of tests has been reduced. The reduction is most probably due to the fuel and seed being introduced simultaneously.

The replacement of the ATJ graphite caps on the electrode walls with pyrolytic graphite caps has resulted in significantly higher surface temperature. As a result, the voltage drop is some 60 percent of the "cold" wall voltage drop during the previous series of tests. However, the absolute value of the present voltage drop is still greater than the original design predictions. Test results indicate, however, that the overall enthalpy extraction objective can be achieved.

I. INTRODUCTION

The overall objective of this research, initiated in late 1973, is to demonstrate the attainment of MHD performance on a sufficiently large scale to verify that commercial application of the MHD concept is a viable energy alternative. To perform the research, an existing facility at Arnold Engineering Development Center (AEDC), Tullahoma, Tennessee, was refurbished and additional systems required for performing the experiment were designed and built. Shakedown tests of the major flow train components and the 6 Tesla (T) cryogenically cooled magnet were complete by late 1979.

Testing with the channel configured in the Faraday mode was initiated in late 1979 and continued into 1980 until a serious electrical failure interrupted the planned test sequence. A comprehensive system repair and improvement effort followed. Faraday testing was resumed in May of 1981 and a total of nine power production runs have been achieved to date. The electrical loading distribution and inlet plasma conductivity have been selected to produce low supersonic, slightly accelerating flow in the channel. The power output has been gradually increased in order to evaluate the influence of selected parameters on both performance and hardware. This report describes the system repairs and presents the test results obtained in FY81.

II. FACILITY DESCRIPTION AND OPERATION

2.1 FACILITY DESCRIPTION

The HPDE facility consists of a high field strength electromagnet, a heat sink channel, a liquid fuel combustor, an exhaust breech containing a water spray bank to quench the plasma, and all peripheral equipment required to successfully operate the facility. The HPDE facility, together with the coordinate convention is shown in Fig. 1.

2.1.1 Magnet System

The HPDE magnet is one of the largest and highest field strength magnets devoted to MHD research. A photograph of the magnet is given in Fig. 2. The magnet is of conventional saddle coil configuration with a steel return frame and is approximately 10 m in overall length. The magnet is designed to provide dual mode operation. For pulsed operation, the upper and lower saddle coils are series-connected and precooled with liquid nitrogen to optimize the impedance match with the power supply. In this configuration, the run duration time at a peak field strength of 6 T is limited to about 40 sec. For the continuous mode of operation, the coils are water-cooled and the upper and lower saddle coils are electrically connected in parallel to provide a peak field approaching 3.5 T.

In order to constrain the large induced magnetic forces, the coils are enclosed in an aluminum force containment structure (FCS). The FCS consists of boxes at both ends which enclose the saddle crossovers. The boxes are joined axially by longitudinal tension members (LTM) located adjacent to the outer sides of the coil. Transverse frames around the LTM and coils complete the structure. During assembly of the magnet, instrumentation sensors comprised of about 150 thermocouples and 150 strain gages were installed at strategic locations on the FCS to monitor the thermally induced strains and the Lorentz force induced strains during peak performance operation. During operation, the magnet current, the applied voltage, and the magnetic field are monitored. The magnetic field is monitored with a matrix of Hall field probes (EH Bell Type BHT 900) installed in the magnet bore.

The magnet is powered by a 27 MW power supply capable of producing 17,000 amp with a tap-changing-under-load capability that allows the open-circuit voltage to be varied without interruption from 1,400 v to 2,800 v. A reactor bank allows pretest insertion of any one of seventeen different reactor bank configurations to provide reactance to limit short-circuit current. Design information and a complete description of the magnet are given in Ref. 1.

Figure 1. Elevation View and Coordinate Convention for the HPDE Flow Train and Magnet

Figure 2. HPDE Magnet

2.1.2 Generator Channel

The generator channel is designed to produce 50 MW of electrical power at a mass flow of 60 kg/sec and a magnetic field of 6 T. Heat sink construction limits operation to a nominal 15 sec. The channel shape is of rectangular cross section (external dimensions 0.82 m wide by 0.60 m high at the inlet and 1.32 m wide by 1.08 m high at the exit), 8.9 m long, fabricated in five sections for ease of handling. The channel consists of an outer pressure vessel lined with a copper heat sink in the form of interior electrodes and insulating walls. The interior insulating walls are comprised of 1.9 cm square pegs end-mounted to the wall with a 1.6 mm refractory-filled gap between adjacent pegs. The copper electrodes span the channel height on either side and are similarly insulated from each other. ATJ graphite caps (Section 1) and pyrolytic graphite caps (Sections 2 through 5) on the electrodes are utilized to promote a high surface temperature, resulting in favorable electrical contact with the plasma. All interior components are mounted to an outer pressure vessel of NEMA G-11 laminated fiberglass. Steel tie bars surround the pressure vessel and provide the required structural integrity.

The channel contains 472 electrode pairs (400 of which are presently being utilized for power production) and can be operated in the segmented Faraday mode or externally connected for diagonal operation. The channel is presently being operated in the Faraday mode. Approximately every tenth electrode pair is instrumented to measure current and voltage characteristics. Forty static pressure ports are provided to measure axial and transverse pressure distributions. Three pressure rakes located in the diffuser are used to measure the total pressure. The transverse voltage distribution (9 to 11 locations across the channel) is measured at five selected axial locations. A detailed description of the channel design is presented in Ref. 2. A photograph of the channel as it rests on its assembly cart is shown in Fig. 3.

2.1.3 Combustor System

The plasma generator used in this experiment is a liquid-fuel combustor using toluene (C_7H_8) to approximate the C/H ratio of coal. The combustor is designed for a nominal mass flow of from 50 to 60 kg/sec, with a stagnation pressure of 5 to 6 atm and a stagnation temperature of approximately 3,000°K. The oxidizer is supplied from high pressure storage vessels which are filled from separate supply systems to achieve any desired N_2/O_2 ratio, and this flexibility, along with the capability to select any desired O/F ratio, allows combustor operation over a wide range of conditions. The fuel/oxidizer mixture is ignited using an ignitor/pilot combustor combination fed from a separate methane/oxygen system. A complete description of the combustor is given in Ref. 3. The seed is injected into the fuel line upstream of the burner manifold as a solution of potassium hydroxide (KOH) dissolved in methanol.

7

Figure 3. Generator Channel Resting on its Assembly Cart Prior to Insertion into the Magnet

In the previous series of tests (late 1979 and early 1980), a heat sink nozzle was utilized in the flow train. During the repair period, a new water-cooled nozzle was designed, fabricated, and installed. This new nozzle will allow an uninterrupted test schedule to be planned and carried out. Erosion of the old heat sink nozzle necessitated the machining and installation of new parts every 3 to 5 tests. Preliminary data on the water-cooled nozzle indicates that it is performing as designed. Adequate water flow has been observed and visual inspections of the nozzle have not uncovered any leaks or other difficulties. The new nozzle is located approximately 35 cm downstream of the old heat sink nozzle location. The new nozzle and its location in the flow train is given in Fig. 4.

2.1.4 Diffuser and Breech

The HPDE diffuser is of copper, heat sink construction with upper and lower walls that are movable to allow a wall angle from 0 to 4 deg. The sidewalls of the diffuser are fixed. These design features allow channel operation at both subsonic and low supersonic Mach numbers over the design mass flow range. The diffuser connects to a breech in which the plasma is quenched by a water spray (See Fig. 1). A photograph of the diffuser and upstream portion of the breech after installation is given in Fig. 5. Both the diffuser and breech are electrically isolated from ground, and both are mounted on casters to facilitate removal.

2.1.5 Load Bank

The power produced during operation is dissipated in a load bank which is a collection of liquid rheostats. A photograph of the HPDE load bank is given in Fig. 6. Each rheostat provides the proper load for one electrode pair when the channel is operated in the Faraday configuration. Each rheostat consists of a polyethylene bucket and two copper plates, the spacing of which can be varied to provide a resistance from 10 to 80 ohm. Adequate water flow through the bucket is provided to dissipate the design power level of about 100 kw per rheostat while limiting the temperature rise in any given rheostat to less than 10°K.

2.1.6 Instrumentation and Data Acquisition

The HPDE data system utilizes a DEC PDP 11/50 computer for data acquisition and an IBM 370 for bulk data reduction and processing. Instrumentation readings from 335 channels of pressure, flow rate, temperature, voltage, current, strain, and magnetic field data are transferred to magnetic tape by the acquisition computer. Data are scanned at 25 samples per second of each instrumentation item.

Figure 4. Elevation of HPDE Flow Train

Figure 5. Installed Diffuser and Upstream Portion of the Breach

Figure 6. Liquid Rheostat Load Bank

Selected instrumentation items are also scanned at a rate of 5,000 samples per second for approximately 0.1 sec at several points during testing. The diagram of the acquisition system shown in Fig. 7 illustrates the options which are available. One option available is to average the data points over a specified number of scans to provide some smoothing and then print the results on hard copy for engineering analysis and permanent record. From the tape, the reduction program converts the data into engineering units for each scan and creates an engineering unit data file. From the data file, the data can be used with an interactive graphics routine to generate data plots of any measured parameter versus any other parameter at any selected scale. This option is used extensively to study performance transients on each test and to analyze the distribution of measured parameters. In the interactive mode, the data can also be corrected and reprinted.

2.2 FACILITY OPERATION

The operation of the facility requires substantial preparation. For example, on a full-powered test ($B > 2.8$ T) the magnet cooldown begins two days before the actual test. Consumables, such as oxygen, methane, and seed, are purchased or prepared several days before a run. The day of the run, preops begin at 0700 in preparation for a 1300 run. After preoperation procedures are completed, the actual run sequence is as follows: (1) Cooling water flows are established. (2) Required purge flows are initiated and other facility interlocks satisfied. (3) The fuel flow is initiated in a bypass loop. (4) The ignitor and pilot are then activated. The rise in ignitor chamber pressure (30 psi) instructs the data acquisition system to begin operation and initiates a timer to correlate all future data events. An annunciator also records pertinent run events (such as valves leaving the close position) to within 0.001 sec. A list of the parameters monitored by this system is given in Table 1. (5) The oxidizer flow is then initiated and concurrently the magnet breaker is closed. (6) Thirty to 60 sec are normally required to reach a quasi-steady-state magnetic field strength, at which time the oxidizer system has stabilized. The seed valve is then opened. (7) Once seed flow is established, the fuel valve is opened initiating full burner operation. A second timer is keyed to the fuel valve leaving the closed position to further correlate the actual run events. A photograph of the HPDE control room highlighting the main control panel is given in Fig. 8, and a typical HPDE operation sequence (Run MI-007-006) is depicted in Fig. 9.

During full operation, some 110 electrical interlocks are utilized to safeguard the system, to the extent possible, from potential difficulties. An automatic shutdown sequence is initiated if any of the parameters interlocked are out of limit. The automatic shutdown sequence includes "crowbarring" the system or shorting the diffuser to ground to limit damage in case of electrical failure. Some of

Figure 7. HPDE Data Reduction System

Table 1

List of Parameters Monitored by Annunciator System

ANNUNCIATOR NO.	PARAMETER	ACTUATING DEVICE
1	RECTIFIER WATER FLOW	RWR1-3
2	METHANE PRESSURE	MR2-3
3	OXIDIZER SYSTEM PRESSURE	OR7-3
4		
5	SPRAY RING WATER FLOW	WR15-3
6	PILOT CHAMBER WATER FLOW	WR21-3
7	PILOT THROAT WATER FLOW	WR19-3
8	IGNITER WATER FLOW	WR18-3
9	BURNER EXTENSION WATER FLOW	WR8-3
10	BURNER WATER FLOW	WR7-3
11	BACKPLATE COOLING	MR8-3
12	PURGE PRESSURE	PR4-3
13		
14	FUEL FLOW (ACTION PACK)	FR4-3
15	IGNITOR ON COMMAND	MR3-3
16	IGNITOR CHAMBER PRESSURE	MR5A-3
17	PILOT ON COMMAND	MR5-7
18	PILOT CHAMBER PRESSURE	MR6-3
19	MAGNET ON	MGR1-3
20	OXIDIZER FLOW (ACTION PACK)	OR8-3
21	OXIDIZER FLOW	OR9-3
22	OXIDIZER VALVE CV6-6 CLOSED	OR5-7
23	FUEL VALVE OPEN COMMAND	FR5-5
24	FUEL VALVE LEAVE CLOSE	FR1-3
25	SEED VALVE OPEN COMMAND	SR1-6
26	CONTROL TIMERS ON	CTR1-3
27	FUEL VALVE FULL OPEN	FR11-3
28	BURNER CHAMBER PRESSURE	FR8-3
29	BURNER CHAMBER HIGH PRESSURE	FR12-3
30	SEED VALVE LEAVE CLOSED	LS4-15
31		
32	TEST STOP BUTTON	TR2-2
33	KILL SWITCH NO. 1	TR3-2
34	KILL SWITCH NO. 2	TR4-2
35	KILL SWITCH NO. 3	TR5-2
36	MAIN TERMINATION RELAY	TR1-7
37	JENNING SWITCH ENERGIZED	TR1-8
38		
39	PURGE VALVE LEAVE CLOSED	FR-13
40	FUEL PURGE PRESSURE	FR-10

Figure 8. System Control Center

Figure 9. Selected Test Data Illustrating a Typical Run Sequence

the pertinent parameters during a run are given in Fig. 10, which depicts the variation with time of fuel flow, seed flow, burner pressure, flame temperature, diffuser voltage, and the Faraday voltage, current, and power on an electrode pair near the center of the channel. The above-mentioned parameters, except the flame temperature, are all measured. The flame temperature is calculated using the equation $T_f = \text{constant } (p/\dot{m})^2$ where p is the stagnation pressure and $\dot{m}$ is the total mass flow. Both the pressure and total mass flow are measured parameters. On this particular run (MI-007-006), the fuel and seed flows reached set point in about 2 sec and flow rates were very stable during the rest of the run. The 2-sec fuel ramp resulted in a smooth "soft" start. No overpressurization in the diffuser was observed. Arcs to ground were also not observed. During a number of previous experiments, arcs to ground at various locations, breech to exhaust stack, load bucket, diffuser casters, etc., were observed. New load buckets, improved diffuser insulators and a 6-ft fiberglass breech spool were effective in eliminating arcs to ground during recent tests. Steady values of current and voltage were obtained and the test was terminated by the automatic timer. The amount of power increases steadily with time up to about 6 sec and this indicates that the temperature of the cap surface is increasing with time. A brief discussion of cap temperature variation with time is discussed in Section 5.

Figure 10. Variation of Selected Key Parameters during HPDE Run MI-007-006

III. FACILITY REPAIR AND IMPROVEMENT

Repair and improvement of the HPDE flow train and its peripheral equipment were completed during this report period and Faraday testing was resumed. The description of the failure which occurred during Run 6-016 has been previously presented (Ref. 4), and the proposed system modifications were detailed in that same document. Specific details and accomplishments have been presented in subsequent progress reports. Those details will not be repeated here, but the governing philosophy will be discussed as well as the degree of success achieved in meeting the proposed repair objectives.

3.1 MAGNET BORE LINER

The damage to the bore liner underscored the possibility of damaging the magnet coils should a massive electrical failure recur. To lessen that probability, the bore sidewalls were replaced with 0.635-cm (0.25-in.) flame retardant fiberglass reinforced laminate (NEMA Grade GPO-3) in lieu of the original 0.32-cm (0.125-in.) G-10 panel.

The original channel support track assembly was designed to prevent an axial short along the length of the assembly. On two occasions, however, such shorting did occur, indicating an inadequate design in terms of electrical isolation. The track assemblies were modified to improve the electrical insulation by eliminating the stainless steel support runners underneath each track assembly, replacing them with fiberglass reinforced laminate, and by replacing the 0.16 cm (0.062 in.) insulators between each rail section with either 2.54 cm (1 in., 5 places) or 15.24 cm (6 in., 3 places) fiberglass reinforced rods. No arcing or tracking has been evident in the nine runs made since upgrading the track insulation.

During the repair period, an expanded array of Hall probes were installed along the magnet floor to more accurately measure the axial magnetic field distribution.

3.2 GENERATOR CHANNEL

Repair of the HPDE generator channel and the associated power distribution system was performed with the intent to significantly reduce the possibility of another electrical failure. It was assumed, however, that another failure could occur, so the approach taken was to upgrade all components so that a reasonable fault current (and associated forces) could be sustained for a time interval long enough to prevent a rapid cascade and long in comparison to the system shutdown time. To accomplish this, the channel wiring was increased in size from No. 12 AWG to No. 10 AWG, improved terminal lugs were used at both the power

takeoff stud and the terminal strip, and the terminal strip was upgraded to withstand 20,000 v. All exposed terminals were eliminated by covering the power takeoff stud region with a fiberglass strip and potting the entire void. The 30-conductor cable was also increased in size from No. 12 AWG to No. 10 AWG and was tested to 45,000 vdc without failure. In general, the wiring system was designed to withstand a minimum of 20,000 v, with insulation rated at 200°C, and sized to carry a 1,000 amp fault current for 2 to 3 sec, adequate time to permit system shutdown. Again, the specific details of the repair are given in Refs. 4 and 5.

During the repair period, the ATJ graphite caps on the first nine rows of electrodes in the channel were replaced with copper caps to eliminate the excessive erosion those caps experienced during the first series of tests. This modification should permit six to eight powered runs before replacement is required. Previously, the caps were replaced every two to three runs.

3.3. PATCH PANEL

Since the previous failure was attributed to an arc from the north patch panel to ground, particular care was taken to upgrade that component. The system was redesigned to utilize one patch panel instead of two, the single panel was mounted on a fiberglass support structure rather than a metallic structure, and all exposed terminals were eliminated. A single-conductor high-voltage connector assembly was used in lieu of the previous exposed terminal studs, and the entire system was tested to 30 kvdc without failure. The pegwall voltage measurements are routed through the patch panel and utilize the same connector assembly. No difficulties have been experienced with the patch panel during the nine tests conducted since resumption of Faraday testing.

3.4. COMBUSTOR SYSTEM

Several operational and mechanical changes were made to the combustor system during the repair and improvement period. A new water-cooled nozzle was procured in order to eliminate the erosion problem experienced with the heat sink nozzle, to improve the plasma flow quality entering the channel, and to improve the overall reliability. Results since resumption of testing indicate that all objectives have been accomplished.

As previously noted, one of the objectives of the electrical system repair was to be able to sustain a fault current for a time period long enough to allow system shutdown. In order to minimize the time required for system shutdown, boosters were installed on the valves in the fuel system to minimize signal to closure time. The present elaborate protective interlock system has provided ample opportunities to evaluate the effect of

the modifications, since several runs have been terminated by system interlocks. The test data show that system shutdown time - from the time an interlock exceeds its preset limit to full closure of the fuel valve - is about 0.8 sec. The previous shutdown time was about 3 sec. Since the electrical system is designed to sustain a fault current for 2 to 3 sec, the present response of the burner system should be adequate to prevent the cascade of an electrical failure and significantly reduce the damage resulting from a local failure.

Changes in the sequence of operation of the burner system have also been made for two purposes: (1) to eliminate the starting pressure surge that has been present in some of the earlier runs, and (2) to eliminate or reduce the voltage transient during startup. On at least two earlier runs, the initial pressure surge has blown the O-ring seals out of the diffuser walls and slightly damaged other diffuser components. To eliminate that surge, the fuel controller has been programmed to give a "soft" burner start, and the GN₂ fuel purge sequence has been modified to help control that start. The initial controller set point is at 60 percent of the final desired flow rate when the fuel valve leaves its closed position and is then linearly increased to the final set point over a 2 sec interval. In the previous test sequence, the GN₂ fuel purge valve got its signal to close simultaneously with the signal to open the fuel valve. Analysis of the transient data revealed that the purge valve operated more rapidly, and the purge pressure in the fuel manifold had already decayed to almost zero prior to the fuel valve leaving its closed position. That resulted in a surge of fuel into the fuel manifold cavity and prevented smooth control by the flow controller. The sequence was changed to delay the purge valve closure signal until the fuel valves leaves its closed position, resulting in a smoother transient as the fuel enters the manifold and effectively pushes out the remaining GN₂ as it fills the cavity. A similar change was made to the termination sequence to purge the fuel manifold and feed lines after fuel valve closure, such that, the fuel flow is essentially uninterrupted until all the fuel is injected into the burner chamber. This change has effectively eliminated the final pressure surge previously experienced. In prior runs there was a slight delay in opening the purge valve, allowing the burner pressure to significantly decay before the purge pushed the last fuel into the chamber, giving a final pressure surge. Burner operation during the present test series has exhibited a much smoother starting and stopping transient.

To eliminate the voltage transient during startup, seed is now injected simultaneously with the fuel. The seed is injected into the fuel line just upstream of the fuel valve. In previous runs, the fuel valve had been opened prior to opening the seed valve. This allowed almost full flow to be established in the burner before seed flow reached the burner, resulting in a voltage transient experimentally determined to be approximately equal to 2,000 B². During the present test sequence, the seed valve is opened prior to opening the fuel valve, allowing seed flow to be established in the fuel bypass line such that fuel and seed are simultaneously switched from the bypass line to the burner. That sequence change, along with the "soft" burner start, has dramatically reduced the magnitude of the voltage transient (Fig. 11). The success of the new operation sequence has

Figure 11. Variation of the Diffuser Voltage to Ground with Time

eliminated the necessity of using a bleed resistor to control the transient voltage spike.

3.5 LOAD BANK

All load bank modifications have been previously reported, but will be briefly repeated here for the sake of completeness. The steel grating underneath all load buckets has been replaced with fiberglass grating, and the steel grating in the walkways has been replaced with fiberglass grating over the high voltage half of the load bank. New polyethylene buckets have been procured and installed, replacing all buckets which are used as an active load. The new buckets plus fiberglass grating system have been tested to 20 kvdc without failure.

All power production instrumentation is now located in the load bank. This eliminates the necessity to terminate any high current carrying wire on the mezzanine inside the building. The current transducer for each instrumented electrode pair is located adjacent to its respective load bucket and only the millivolt signal to the signal conditioning system is transmitted inside the building. The anode-to-ground and cathode-to-ground voltage measurements are also accessed at the load bank, requiring only low current carrying capacity wiring to be terminated inside the HTL building.

3.6 DIFFUSER AND BREECH

The pressure seal between the exhaust breech and ground potential exhaust stack is made using a 25-cm flexible rubber "collar," and has historically experienced repeated arcs to ground. To lengthen the arc path to ground through the seed laden steam environment at the breech-stack interface, a 1.83-m fiberglass spool piece was procured and installed on the downstream end of the exhaust breech. Therefore, total arc path length at the breech-stack interface is now approximately 2 m as compared to an arc path length of 25 cm during the last series of tests. During the present test sequence, the diffuser and breech to ground voltage has remained constant with no hint of inadequate electrical integrity.

3.7 MODIFIED CONTROL PANEL

A modified control panel was installed, allowing all system components except the magnet, to be operated from a single point. The panel incorporates an emergency stop button for the operator, as well as three remotely held emergency stop buttons which are held by personnel assigned to monitor specific system components. The new control panel has resulted in a smooth test unit operating sequence while allowing the test unit engineer increased control of the experiment.

3.8 INSTRUMENTATION IMPROVEMENT

The specific details of the instrumentation changes have been previously reported (Refs. 4 and 5). The total number of digital data acquisition channels has been increased from 164 to 335, with the primary emphasis on the accurate measurement of the generator electrical characteristics. The number of monitored electrodes was increased from 10 to 50, allowing approximately every tenth powered electrode to be monitored along with additional measurements near the pegwall voltage locations. The number of pegwall voltage measurements was increased from 15 to 31 in Channel Sections 2, 3, and 4 to more accurately determine the transverse voltage distribution in the peak power production portion of the channel. The pegwall voltage measurements allow the electrode voltage drop to be more accurately defined.

3.9 CHANNEL FLANGE LOADS AND PRELOAD SYSTEM

Quite early in the previous test sequence, it was discovered that the strength of the channel inlet flange and the strength of the flange between Channel Sections 1 and 2 were marginal considering the total anticipated loads to which those flanges are exposed during high interaction experiments. The concern over flange integrity was heightened by the knowledge that the severe design constraints and limited available space precluded the normal use of generous safety factors. To prevent possible compromise of the flange integrity, a detailed analysis was performed to determine the actual forces on the flanges at various run conditions. The details of that analysis were presented in Ref. 6. The results of the analysis indicated that the flange strengths were adequate to withstand the aerodynamic forces at any run condition, but had marginal strength to withstand the combined aerodynamic and magnetic forces at magnetic fields above 3.2 T. As a result, a preload system was designed, fabricated, and installed prior to resumption of the present test sequence.

The "as-built" preload system is illustrated in Fig. 12. The system consists of a double polyester strap, under tension, connected between the technical flange (just upstream of the inlet flange) and the diffuser flange. Polyester was used because of its high mechanical strength, relatively low elasticity and high dielectric constant. The strap is fixed in two brackets located at the diffuser flange and technical flange. The strap is permanently fixed in the bracket at the technical flange, while the bracket of the diffuser flange holds the strap loosely during the loading process and is then tightened to transfer the load from the loading assembly to the channel. The straps are loaded by attaching additional brackets to the strap ends which are, in turn, attached to the south wall of the HTL building with 1.43-cm steel cable. A hand-operated winch and dynamometer are utilized to apply the appropriate loads. After the predetermined load is applied, the strap is firmly clamped in the

Figure 12. Preload System

downstream bracket and the loading assembly is removed, leaving the channel in compression at a predetermined load. The load selected depends on the run conditions.

The preload system has been successfully used during the present series of tests and preinstallation material integrity tests have proven to be adequate and accurate. One strap failure did occur, however, during Run MI-007-007. An electrical arc occurred during Run MI-007-007 burning the straps and causing a failure. The electrical wiring problem has now been corrected and no future problems are anticipated with the preload system.

3.10 MISCELLANEOUS SYSTEM CHANGES

Several miscellaneous changes were performed to increase system integrity, provide additional system protection, or improve operational characteristics. The additions include an expanded TV monitoring system utilizing seven monitoring locations. In addition, video recordings can be made from any four preselected monitors. A new remotely operable Halon fire extinguisher system was procured and installed with discharge points at the burner, in the annulus between the channel and magnet bore, on both the north and south face of the patch panel, and at the exit of the magnet bore. This system not only provides protection in areas not previously covered, but also reinforces the existing dry powder system in the burner area.

A fiber optic arc detection system was installed in the magnet bore, at the patch panel, and at the signal conditioning boards on the mezzanine. Two data acquisition channels are delegated to monitor the fiber optics output and initiate system shutdown should a preselected output level be exceeded.

An additional magnet protection system, in the form of a sphere gap, was installed between the positive and negative power leads at the point where those leads are connected to the buswork going to the magnet power supply. The sphere gap is experimentally set at 5 kv and will protect the magnet coils should a fault occur on the power supply side of the sphere gap (open in the feed lines to the magnet). The sphere gap will, of course, not effectively eliminate or limit damage should the fault occur on the magnet side of the sphere gap installation.

The high voltage protection system on the downstream end of the flow train was modified to provide a more positive control of the system voltage. During system startup, the diffuser is electrically grounded through a Jennings switch until the seed valve leaves its closed position. This prevents inadvertently starting the system without seed flow, which could result in a excessive voltage transient. Additionally, should the seed valve close during a run (perhaps due to a loss of gaseous nitrogen pressure to the valve controller), the Jennings switch would "crowbar" the system and

initiate shutdown. A current transducer on the ground side of the Jennings switch provides the signal for system shutdown. Shutdown is initiated if any current flows in that circuit. The Jennings switch is electrically wired as a fail-safe device, which means that the coil must be energized to open the switch and unground the diffuser. If the power to that circuit is interrupted the Jennings switch will close and initiate shutdown.

The Jennings switch is used as a primary protection device for the entire HPDE flow train. All electrical wiring, connections, and insulation were designed and tested to withstand a minimum of 20 kvdc during repair and if that value is exceeded during a run, system shutdown is initiated and the Jennings switch closed. If a run is terminated by any interlock exceeding its limit, the Jennings switch is automatically closed. This rapid grounding of the high voltage end of the channel will bleed off the system voltage and reduce the amount of damage sustained in an electrical failure.

The Jennings switch is also used in the termination of a normal run sequence. The first two or three runs in the present test sequence were performed with the seed flow continuing to the very end of the run to prevent a voltage spike at the end of the run. Inspection of the channel interior revealed that seed deposition was occurring on the channel walls, resulting in a dramatic decrease in interelectrode resistance. To prevent continued channel degradation, subsequent runs were conducted with the seed valve timed to close approximately 1 sec prior to fuel valve closure. This allows the seed to be completely swept from the channel while the plasma flame temperature is still high. Without preventive measures, a voltage spike would certainly result from such a sequence. The Jennings switch closes upon seed valve closure, however, grounding the system and preventing any shutdown electrical transients.

Redundant protection is provided in the "crowbar" circuit in the form a sphere gap installed parallel to the Jennings switch and experimentally set to arc at about 21 kv. Should the Jennings switch circuit fail to respond for any reason, the sphere gap would arc, again grounding the diffuser and initiating system shutdown.

3.11 SYSTEM SAFETY HAZARD ANALYSIS

In view of the extensive failure experienced during Run 6-016 and the major system modifications made pursuant to that failure, an indepth system safety analysis and risk assessment was made for the entire HPDE system (Ref. 7).

Each potential hazard to a test component, subcomponent, or to operating personnel was considered, the severity of such a hazard evaluated, and the probability of its occurrence estimated. For each hazard, the preventive measures that had been taken or could be taken to eliminate

the hazard or reduce its severity and/or probability were addressed. It was concluded that the safety of the operating personnel was completely adequate and that all reasonable precautions to prevent equipment damage had been taken. It was further concluded, however, that the developmental nature of the hardware and the severe operating conditions of the experiment are conducive to periodic failures. The hazards and preventive measures were then further analyzed to ensure that all possible precautions had been taken to localize and minimize damage that might occur from future failures..

Although previously reported, two items from the system safety analysis should be noted: (1) channel failure in the form of interelectrode arc-over could occur at the high interelectrode voltages which will result during operation at high magnetic field strengths; and (2) magnet failure could occur due to an internal short in the magnet coil or a short from the magnet coil to the force containment structure. Those two hazards are well identified, but no preventive measures are available to reduce the risk without significant advances in the state of the art.

IV. MAGNET PERFORMANCE

4.1 QUASI-STEADY-STATE MAGNET PERFORMANCE

The magnet has been tested by itself as well as with the MHD HPDE generator channel installed under power-producing conditions during the last year. All tests have been in the pulse mode of operation with coils series-connected; the test conditions are summarized in Table 2. The tests are categorized as "warm" and "cold," referring to the general temperature of the coils. For the "warm" tests, the coils are initially at room temperature. The impedance mismatch with the power supply in conjunction with the thermal mass of coils results in only a modest temperature rise of approximately 10°K in the 40 sec required to establish a quasi-steady-state field. With the coils warm and series-connected, the range in field peak value is from a minimum of 1.5 T to a maximum of 2.8 T.

In general, the range of B-field strengths which can be achieved at a particular temperature is approximately 1.2 T to 1.5 T. For the "cold" tests, the coils of the magnet are precooled to a predetermined temperature with LN_2 . The coolant flow rates are controlled to allow for thermal relaxation and the cooldown is typically accomplished over a two-day period. Gaps in the structure accommodate any differential thermal expansion. Cooling the coils for tests conducted to this point has been accomplished without actively cooling the FCS, however, the FCS is partially cooled by conduction from the coils. Extremely high magnetic field tests may require active cooling of the FCS.

During operation, the magnet current, applied voltage, developed field and selected strain gages are monitored. The Hall field probes (EH Bell type BHT 900) are mounted along the magnet bore floor underneath the channel. The gages are mounted on a plastic frame positioned in the bore centerplane parallel to the ground. Previous correlation studies of the magnetic field on the bore floor and centerline make it possible to calculate the magnetic field at the bore centerline as a function of axial location during a power producing test. A planview of the gage locations is shown in Fig. 13. The probe positioned at 2.0 m corresponds to the location of maximum expected field. The probes used are uniaxial and are oriented to respond to only the vertical field component.

The performance of the magnet can be most simply summarized by its magnetization curve giving the magnetic field strength at a particular location in the bore as a function of coil current. A comparison of a magnetization curve based on the original design equations, Ref. 1, with some of the data obtained to date at the peak field location in the magnet bore is given in Fig. 14. The data points indicated in Fig. 14 were all taken near the pulse peak when the temporal gradient

Table 2
Magnet Run Summary

RUN NO.	COIL TEMPERATURE °K	DURATION, sec	V _{oc} , v	I _{max} , amp	PEAK* B-FIELD, Tesla	FIELD POSITION
MI-004-002	300	49.5	1,400	2,801	1.82	ϕ
-003	↓	46.9	2,100	4,066	2.30	↓
-006	↓	43.7	1,400	2,813	1.78	↓
-007	↓	42.0	2,100	4,010	2.32	↓
-008	↓	30.8	2,800	4,917	2.69	↓
MI-005-002	160	52.1	1,400	5,437	2.95	ϕ-Floor
MI-006-005	300	36.1	1,400	2,400	1.59	Floor
-006	↓	42.5	1,400	2,402	1.59	↓
-008	↓	48.3	2,100	3,990	2.36	↓
-009	↓	46.6	2,800	4,920	2.71	↓
-010	↓	41.0	2,800	4,890	2.70	↓
-011	↓	35.8	2,800	5,070	2.77	↓
-012	↓	44.5	2,800	5,145	2.80	↓
-013	↓	53.6	2,800	5,105	2.78	↓
MI-006-014	155	48.3	1,540	6,352	3.20	Floor
-015	175	53.0	2,330	7,430	3.48	↓
-016	160	48.5	2,580	8,640	3.92	↓
MI-007-001	140	71.8	2,370	9,040	4.10	ϕ-Floor
MI-007-002	300	36.1	1,710	3,310	2.10	Floor
-003	↓	40.7	1,710	3,380	2.14	↓
-004	↓	40.5	1,710	3,290	2.12	↓
-005	↓	43.5	2,800	5,020	2.79	↓
-006	↓	49.1	2,800	4,950	2.75	↓
MI-007-007	240	52.9	2,800	6,066	3.10	Floor
-008	160	68.7	2,370	7,440	3.55	↓
-009	150	37.4	2,760	5,118	4.03	↓

*Maximum B-field 2.0 m downstream of inlet faceplate.

NOTE: All dimensions in meters.

Figure 13. Location of the Hall Probes on the Floor of the Magnet Bore

Figure 14. Steady-State Magnet Characteristics

of the field strength is small and steady-state conditions can be assumed. A detailed design analysis of the magnet was performed only for the two operating points, 3.5 T and 6.5 T, and the details of the magnetization curve for the low field strengths where the steel becomes saturated were not considered. However, the experimental data are consistent with the slope of the design magnetization curve at fields greater than 3T. It appears that the peak performance attainable may be slightly less than predicted in the design analysis for a given coil current.

The distribution of the field strength down the magnet bore is shown in Fig. 15 for various peak field values along with the design prediction for 3.5 T. The shape of the measured profiles is in good agreement with predictions. Some of the data in Fig. 15 are for floor-mounted probes as well as centerline-located probes. Though not specifically shown in the figure, laterally off-axis probes have indicated slightly higher field strengths than the center value. Testing with the channel installed at the higher fields has limited field uniformity measurements; however, preliminary data indicate that the magnetic field at the bore centerline to be approximately 3 percent higher than the field at the floor of the bore.

4.2 STRESS ANALYSIS

An analysis of the stress buildup in the FCS utilizing strain gage measurements obtained during operation is continuing. The objective of the analysis is to evaluate the stress distribution in the FCS in order to assure that the structure will not be overstressed as higher performance levels are achieved. The measured strains are compared with predicted strain profiles obtained from the SAP 4 finite element model. The predicted strain profiles are determined assuming the magnetic load to be proportional to the product of field strength, B (T), and current, I (amp). The total axial load at 6 T was assumed to occur at $I = 17,000$ amp. Strain gages are attached to selected members expected to have the highest stresses, located primarily on the magnet outlet end and along the left (east) side. Gages are located on the longitudinal tension members (LTM), the outlet faceplate (OFP), the upper outlet tongue (OTNG), the upper transverse tension member (TTM), the upper outlet saddle cap (OSC), and the left outlet sideplate (OSPL). Gage locations are illustrated in Fig. 16 with exact locations and orientations for each structure specified in Figs. 17 through 22.

The strain gages used are uniaxial gages mounted to respond to the longitudinal strains, with a positive strain reading indicating a gage in tension and a negative reading, compression. The strain gages are chosen to have self-temperature compensation and negligible magnetoresistive effects over the expected 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.

Figure 15. Axial Field Distribution in Magnet

Figure 16. Location of Strain Gage Instrumentation on HPDE Magnet Force Containment Structure

Gage.	Z, in.	Y, in.	Z, cm	Y, cm
LTM-L- 1	12.25	0.50	31.12	1.27
2	12.25	5.03	31.12	12.78
3	12.22	9.46	31.04	24.03
4	17.87	10.00	45.39	25.40
5	12.22	15.54	31.04	39.47
6	12.22	20.00	31.04	50.80
7	12.19	24.45	30.95	62.10
8	17.97	30.01	45.64	76.23
9	12.22	30.55	31.04	77.60
10	12.22	35.01	31.04	88.93
11	42.59	10.03	108.43	25.48
12	42.60	30.03	108.20	76.28
13	140.35	10.00	356.49	25.40
14	140.35	30.06	356.49	76.35
15	139.57	10.02	354.51	25.45
16	139.63	30.06	354.66	76.35
17	237.89	30.04	604.24	76.30
18	351.90	9.33	891.54	25.22
19	351.93	30.39	893.90	76.43
20				
21				
22				
23				

NOTE:

*Inside surface

Gages on outside surface unless noted.

Gages oriented to measure strains in flow direction.

Strain gages are located at corresponding locations on the right Longitudinal Tension Member.

Figure 17. Strain Gage Locations on the Left Longitudinal Tension Member

Figure 18. Strain Gage Locations on the Outlet Faceplate

Gage	Z in. (Upstream Edge)	X in. (Ref. Centerline)	Y in. (Ref Top Surface)	θ deg (Ref Flow)	Z cm (Upstream Edge)	X cm (Ref. Centerline)	Y cm (Ref Top Surface)
OTNG-U- 1	11.00	27.69	2.49	0	27.94	70.33	6.32
2	11.04	24.44	0	0	28.04	67.16	0
3	10.99	22.84	0	-45	27.91	47.85	0
4	10.97	22.49	0	0	27.86	46.96	0
5	10.97	22.18	0	90	27.86	46.18	0
6	10.99	11.53	0	0	27.91	29.29	0
7	11.01	0.06	0	0	27.96	0.15	0
8	10.97	-11.54	0	0	27.86	-29.31	0
9	11.00	-12.15	0	90	27.94	-46.10	0
10	11.00	-12.47	0	0	27.94	-46.91	0
11	11.02	-12.84	0	+45	27.99	-47.85	0
12	11.04	-22.44	0	0	28.04	-67.16	0
13	10.87	-27.69	2.45	0	27.61	-70.33	6.22
14	30.5	25.44	0	0	77.47	67.16	0
15	30.5	0.00	0	0	77.47	0	0
16	30.5	-25.42	0	0	77.47	-67.11	0
17	30.50	25.40	*	0	77.47	67.06	*
18	30.50	1.00	*	0	77.47	0	*
19	30.53	-25.44	*	0	77.55	-67.16	*
20	55.62	-5.06	*	-45	141.27	-15.39	*
21	55.62	-5.41	*	0	141.27	-16.23	*
22	55.59	-5.69	*	30	141.20	-16.99	*
23	58.11	27.69	3.16**	0	147.60	70.33	3.03**
24	58.09	-27.69	0.36**	0	147.55	-70.33	0.11**
25	58.12	-27.69	3.38**	0	147.62	-70.33	3.15**

*Inside surface

**Measured from lower surface

NOTE: Strains measured along
long gage axis

Figure 19. Strain Gage Locations on the Upper Outlet Tongue

	Z in.	X in.	Y in.	θ deg	Z cm	X cm	Y cm
TTM-U10-1	6.0	-26.0	4.0	-45	15.24	-66.04	10.16
- 2	6.0	-25.75		0	15.24	-65.40	
- 3	6.0	-25.5		90	15.24	-64.77	
- 4	0.5	0.0		90	1.27	0	
- 5	6.0	25.5		90	15.24	64.77	
- 6	6.0	25.75		0	15.24	65.40	
- 7	6.0	26.0		45	15.24	66.04	
- 8	16.0	-26.0		-45	40.64	-66.04	
- 9	16.0	-25.75		0	40.64	-65.40	
-10	16.0	-25.5		90	40.64	-64.77	
-11	19.0	0.0		90	48.26	0	
-12	18.7	0.0		0	47.50	0	
-13	16.0	25.5		90	40.64	64.77	
-14	16.0	25.75		0	40.64	65.40	
-15	16.0	26.0		+45	40.64	66.04	
TTM-U10	-16	0.0	2.7	90	0	0	6.86
TTM-U10-0-17	6.03	-26.13	0.0	-40	15.32	-66.37	0
-18	6.00	-25.77		0	15.24	-65.46	
-19	6.00	-25.46		+91	15.24	-64.67	
-20	0.52	-0.06		+91	1.32	-0.15	
-21	6.06	25.45		90	15.39	64.64	
-22	6.05	25.95		-01	15.37	65.91	
-23	6.00	26.15		+39	15.24	66.42	
-24	16.10	-26.08		-38	40.89	-66.24	
-25	16.05	-25.73		-01	40.77	-65.35	
-26	16.06	-25.45		89	40.79	-64.64	
-27	18.76	0.00		0	47.65	0	
-28	19.08	0.00		90	48.46	0	
-29	16.05	25.41		90	40.77	64.54	
-30	16.06	25.69		0	40.79	65.25	
-31	16.13	26.06		43	40.97	66.19	

*Inside Surface

NOTE: Strains measured along
long gage axis

Figure 20. Strain Gage Locations on Transverse Tension Member, Upper 10

		X, in.	Y, in.	θ , deg	X, cm	Y, cm
Strain Gage	OSC-U-1	4.68	22.95	53	11.89	58.29
	-2	5.00	23.02	91	12.70	58.47
	-3	5.30	23.02	02	13.46	58.47
	-4	58.47	10.06	02	148.51	25.55
	-5	58.56	35.98	-01	148.74	91.39
T/C OSC-U-	1	2.00	23.0		5.08	58.42
	2	58.5	23.0		148.59	58.42
	3	115.0	23.0		292.10	58.42

UPPER SADDLE CAP
Strain Gage and Thermocouple
Locations

Upper Outlet Saddle Cap (OSC-U---)

Figure 21. Strain Gage Locations on the Upper Outlet Saddle Cap

Gage	Z in.	Y in.	θ deg	Z cm	Y cm
OSP-L-0-1	19.55	32.21	135	49.66	81.81
-2	19.87	32.20	89	50.47	81.79
-3	20.20	32.23	01	51.31	81.86
-4	25.47	34.90	135	64.69	89.65

Figure 22. Strain Gage Locations on Left Outlet Sideplate

The stresses measured along the LTM, in general, correlate well with predicted values (Fig. 23). Gage locations No. 5 to 7, however, on the middle finger measured strains that exceed predicted values. As predicted by the FCS stress analysis, however, the critical finger stress occurs near Gage No. 3. Therefore, the strain at Gages No. 5 to 7 will still be acceptable at higher field strengths as long as it stays within the limits of the Gage No. 3 profile. The total axial loading of the FCS is indicated by the centrally located LTM gages No. 14 to 17. Values obtained from these gages were averaged to determine an axial membrane stress which is directly proportional to the total axial magnetic load carried by the FCS. Measured strains lag predicted values, indicating the coil assembly to be reacting more than the conservatively assumed 10 percent of the total magnetic load.

Measurements taken with gages located on the LTM fingers, between the faceplate and sideplate interfaces, indicate the left LTM to be taking a higher load than the right LTM. The total FCS loading, however, as indicated by Gages 14 to 17, appears to be uniformly distributed between the left and right LTM. Gages No. 4 and 8 indicate negative values versus predicted positive strains. A possible explanation could be gaps due to tolerances in the various load path interfaces that are not fully closed at the lower B fields.

Measured strains on the outlet faceplate are consistently less than predicted values, even at the predicted local plastic zones (at 6.0 T) located near the inside corners (see Fig. 24). All strains measured on the upper outlet tongue are within predicted profiles except for Gage No. 13 (Fig. 25) located adjacent to a TTM connecting groove.

Some strain levels beyond those predicted have been observed on the upper TTM (Fig. 26). These, however, are not considered serious unless the trend escalates at higher field strengths. Four gages on the TTM upper surface (Gages No. 17, 19, 25, 27) are indicating strains opposite in sign to those predicted, indicative of a load condition not accounted for in the model or improperly installed gages. In any event, the absolute magnitude of the measured strain is close to that predicted and no overstress condition exists. The strain measured by Gage No. 28 (Fig. 26) is considerably greater than predicted. However, its rate of increase will probably not accelerate at a higher field strength because of interactions at the tongue mating grooves (because of their large bending stiffness, the grooves will tend to load up more rapidly at their end points as the load is increased).

The measured data from the saddle cap (See Fig. 27) indicate, in general, a reversed loading from that predicted. Gage No. 4 indicates tensile strain and Gage No. 5 compressive strain, implying that the saddle cap is actually pushing against the faceplate instead of restraining it. Saddle cap gages will be monitored during future higher magnetic field testing to note any tendency toward strain reversals as substructure gaps continue to close.

Figure 23. Axial Strain along the Surface of the Left and Right Longitudinal Tension Members

Figure 24. Strain Measured on the Outlet Faceplate

Figure 25. Strain Measured on the Upper Outlet Tongue

Figure 26. Strain Measured on the Upper Transverse Tension Member

Figure 27. Strain Measured on the Upper Outlet Saddle Cap

Gages located on the sideplate have measured strain with absolute magnitude somewhat higher than predicted (except Gage No. 3), but still relatively small (less than 250 microstrain at 4.2 T) and not critical. The results are illustrated in Fig. 28.

The overall updated FCS strain data obtained for tests conducted to 4.2 T, generally indicate the structure to be reacting to less than 90 percent of the total axial magnetic load. Therefore, the 6.0 T allowable field strength previously specified is still projected to be a safe design condition.

Figure 28. Strain Measured on the Left Sideplate

V. FARADAY GENERATOR PERFORMANCE CHARACTERISTICS

Experimental results have been obtained with the channel configured in the Faraday mode. Since many changes were made to the system during the repair period, the initial tests were designed to essentially re-qualify and check out the system. The resistive loading was selected to give a low supersonic velocity over the entire channel. Actual experimental values of the resistive loading parameter, RL (resistance times electrode width), measured on instrumented load cells during Runs MI-004-004, -006, and -007 are shown in Fig. 29. The channel is gradually unloaded over the last 0.6 m to prevent unacceptable voltage gradients in the magnet fringe field. The Mach number at the first loaded electrode is about 1.35 (1,200 m/sec) and between 1.5 and 2.0 at the channel exit, depending on the magnetic field strength. Mass flow for these tests was 50 kg/sec with an oxygen-to-fuel ratio and seed percentage chosen to give a normal conductivity of 10 S/m at the channel inlet.

The power output is increased by increasing the magnet field strength on each successive run. To date, tests at magnet field strengths of 2.1 T, 2.8 T, and 3.1 T have been conducted. A summary of important parameters for these first three powered runs (pyrolytic graphite caps) is given in Table 3.

Table 3

Parameter Summary of the First Three Powered Runs
(Pyrolytic Graphite Caps)

RUN NO.	TOTAL FLOW, kg/sec	N ₂ /O ₂	SEED %	σ_I , S/m		PEAK FIELD Tesla	DURATION sec	POWER MW
				Meas	Pred			
MI-007-004	49.3	0.89	1.18	9.2	10.5	2.1	3.6	13.2
MI-007-006	49.6	0.89	1.18	10.3	10.9	2.8	7.0	27
MI-007-007	51.0	0.89	1.18	10.4	10.9	3.1	7.5	30.5

5.1 POWER PRODUCTION

A presentation of the power production results obtained during the present test series is given in this section. As illustrated in Fig. 30, the voltage on the cathode and anode walls rises uniformly along the channel. The Hall voltage builds to about 7,000 v, 8,500 v, and 9,500 v for Runs MI-007-004, -006, and -007, respectively with all regions of the

Figure 29. Resistive Loading Distribution

Figure 30. Development of Cathode and Anode Voltage along the Channel Walls

Figure 30. Continued

c. B = 3.1 T

Figure 30. Concluded

channel developing a comparable Faraday voltage. The Faraday voltage, current and power are given in Fig. 31 for a magnetic field strength of 2.1 T, 2.8 T, and 3.1 T. The net terminal, or Faraday voltage, is fairly uniform over a major portion of the channel with the magnitude of the voltage increasing as the field strength is increased. The Faraday voltage increases toward the open-circuit voltage, UBD, as the resistance of the load is increased over the last meter of the active region of the channel. The Faraday current is also relatively uniform along the channel. The discontinuous changes correspond to the locations of electrode width change. The Faraday power, a product of the Faraday voltage and the current, develops uniformly along the channel with all regions producing acceptable power. A power level of about 90 kw has been achieved on several electrode pairs and the total power for the three runs was 13.2 MW, 27 MW, and 30.5 MW (Table 3).

The results from a one-dimensional calculation of MHD performance for the approximate plasma and magnetic field conditions of the runs are also shown in Fig. 31. A simple voltage drop is incorporated in the program to account, in a very approximate manner, for the hot pyrolytic graphite electrode walls of the present experiment. The voltage drop varies linearly from about 200 v at the channel inlet to about 900 v at the channel exit. The experimental trends and magnitudes of the various power production parameters are in good agreement with theoretical expectations.

A summary of the enthalpy extraction for Faraday tests (both cold wall and hot wall) is given in Fig. 32. The electrical power extracted in the MHD channel (ΔH) is presented as a percentage of the estimated plasma enthalpy entering the electrical loaded region of the channel (H_1) versus the Faraday power production parameter, $\sigma_1 U_1^2 B_{\max}^2$. A heat loss of 7.5 percent, based on experimental data, is assumed between the combustor and the first loaded electrode in determining H_1 . Further details concerning the determination of H_1 are given in Appendix A. The inlet conductivity, σ_1 , is estimated from experimental measurements and the inlet velocity, U_1 , is calculated for an isentropic expansion from the combustor throat to the entrance of the electrically loaded region. On previous cold wall tests, a maximum enthalpy extraction of about 9 percent was achieved at 3.5 T and a σ_1 of 8 to 9 S/m. The nominal distribution of the resistive loading parameter, RL, along the channel was 0.8 ohm-m. The experimental increase in enthalpy extraction with increasing power production parameter is in good qualitative agreement with theoretical trends.

Recent tests with pyrolytic graphite caps on the electrode walls to increase the wall temperature were conducted with a resistive loading of approximately 0.5 ohm-m. A resistive loading of this magnitude, at the magnetic field conditions in question (2.1 T, 2.8 T, and 3.1 T) closely

Figure 31. Development of Faraday Voltage, Current, and Power along the Channel, B = 2.1, 2.8, and 3.1 T

Figure 32. Enthalpy Extraction for Faraday Mode Test

matches the theoretical ideal loading predictions. The maximum enthalpy extraction expected, as obtained from Ref. 8, has been plotted versus the resistive load, R_L , and is given in Fig. 33. Note that at a resistive loading of 0.5 ohm-m and magnet field strengths on the order of 3 T, the channel (hot walls) will extract near the maximum amount of energy possible.

The enthalpy extraction obtained is shown in Fig. 32, along with the theoretical performance predictions utilizing the approximate plasma and magnetic conditions of the tests as well as the "hot wall" voltage drop model. A maximum enthalpy extraction of about 11 percent has been achieved (30.5 MW) at a magnetic field strength of 3.1 T. The agreement between the experimental results and the theoretical expectations is very good, and indicates that the HPDE extraction goal of 15 percent is achievable.

The static pressure distribution along the channel and diffuser walls is given in Fig. 34, together with a one-dimensional calculation which encompasses the present test conditions. The expansion of the supersonic plasma in the channel, as reflected in the static pressure, is influenced by power extraction at these levels. For example, at a location near the center of the channel (~ 4 m), the static pressure varies from 0.7 bars (power extraction level 13.2 MW) to 1.1 bars (power extraction 30.5 MW). At constant mass flow rates, further increases in static pressures are anticipated as power extraction levels increase. The Mach number at the channel exit ranges from 2.2 to 1.6 for recent "hotwall" tests. The Hall parameter $\omega\tau$ is estimated to be 2.2 near the generator exit for the highest power level tests. The recovery process, which starts through an oblique shock system for these supersonic flow test conditions, is initiated only a few centimeters (2.8 T and 3.1 T tests) to about 1 meter (2.1 T test) downstream of the last electrically loaded electrode. The location of the recovery process is dependent on the actual quantity of power extracted from the gas. In order to maintain supersonic flow conditions over the electrically loaded region of the channel during future higher power level tests, the mass flow will be increased to offset the tendency of the recovery process to move upstream as the power level increases. With a proper selection of the mass flow, it should be possible to keep the shock out of the electrically loaded region. The static pressure distribution is also measured perpendicular to the flow at several axial locations. Although the data are not all inclusive, it does indicate that an oblique shock was located just downstream of the last electrically loaded electrode during the last two tests. The transverse variation of the static pressure will continue to be monitored and studied during future tests.

5.2 TRANSVERSE VOLTAGE CHARACTERISTICS

The transverse voltage is measured across the insulator wall (pegwall) in the HPDE channel at the five axial locations shown in

Figure 33. Maximum Expected Enthalpy Extraction

Figure 34. Static Pressure Distribution along Channel and Diffuser Walls

Fig. 35. During previously conducted "cold wall" tests, the transverse voltage was measured at the same five axial locations, but during recent "hot wall" tests, much better definition in transverse voltage measurements was achieved. As noted in Fig. 35, the transverse voltage is measured at 9 to 11 locations at each axial location. This provides a better definition in the transverse voltage, especially near the wall, and enables a more accurate determination of the voltage drop at each axial location.

The transverse voltage distribution for four of these axial locations (Run MI-007-006) is given in Figs. 36-39. It can be seen from this 2.8 T run, that:

- (1) The voltage drop near the electrode walls increases substantially as a function of axial location down the channel.
- (2) The voltage drop at the anode and cathode are quite dissimilar, and
- (3) The voltage drop decreases as time increases (electrode surface temperature increases).

The experimental observations of the voltage drop, utilizing pyrolytic graphite caps on the electrode walls (hot wall conditions) are also shown in Fig. 40, along with the voltage drop data obtained utilizing ATJ graphite caps for comparison. The total voltage drop increases uniformly with distance down the channel from about 350 v at the first pegwall location to about 900 v at the channel exit. The drop on the anode wall is typically about one-half the cathode wall drop except at the front of the channel. Within the estimated uncertainty bands, the voltage drop is essentially invariant over the range of magnetic field strengths tested. The overall average voltage drop in the channel is approximately 60 percent of the previously observed drops. This reduction in voltage drop from cold to hot wall conditions is consistent with theoretical predictions (Ref. 8). Power production and enthalpy extraction calculations, which utilize the new "hot wall" voltage drops, indicate that the overall enthalpy extraction objectives can be achieved.

To further illustrate how the data show that the voltage drop depends on the electrode surface temperature, the voltage drop is presented in Fig. 41 as a function of time and therefore electrode surface temperature for four axial locations. An estimated surface temperature is given for each axial location as a function of time. As the estimated temperature increases by about 300°K, the average voltage drop decreases by about 200 v. An estimate of the electrode surface temperature distribution along the entire channel length for the approximate conditions of these runs is given in Fig. 42. These

PLAN VIEW OF CHANNEL SHOWING LOCATION AND DESIGNATION
OF PEGWALL VOLTAGE MEASUREMENT POINTS IN TOP INSULATING WALL

Figure 35. Plan View of Channel Showing Location and Designation of Pegwall Voltage Measurement Points in Top Insulating Wall

Figure 36. Voltage Distribution across the Channel 1.447 m Downstream of Channel Inlet

RUN: MI-007-006
PEGWALL STATION 24

Figure 36. Concluded

RUN MI-007-006
PEGWALL STATION 93

Figure 37. Voltage Distribution across the Channel 2.79 m Downstream of Channel Inlet

RUN: MI-007-006
PEGWALL STATION 93

Figure 37. Concluded

Figure 38. Voltage Distribution across the Channel 4.63 m Downstream of Channel Inlet

RUN: MI-007-006
PEGWALL STATION 207

Figure 38. Concluded

RUN: MT-007-006
PEGWALL STATION 322

Figure 39. Voltage Distribution across the Channel 6.279 m Downstream of the Channel Inlet

RUN MI-007-006
PEGWALL STATION 322

Figure 39. Concluded

Figure 40. Variation of the Measured Voltage Drop on the Anode and Cathode Walls along the Length of the Channel

Figure 41. Variation of Total Voltage Drop with Time (Surface Temperature)

Figure 42. Surface Temperature Distribution along HPDE Electrode Wall

surface temperature estimates were obtained from previous experimental data and arc heater tests conducted to select a "hot wall" cap material, Ref. 4. During a 7 sec run, the electrode surface temperature averages approximately 1,500°K over the active region of the channel. Theoretical predictions of power production and enthalpy extraction most closely correlate with experimental results when the wall temperature, with the appropriate voltage drop, is set at approximately 1,400°K to 1,500°K. The theoretical predictions, therefore, substantiate to some degree at least that the average wall temperature during a 7 sec run in the present test series is approximately 1,500°K.

VI. SUMMARY

Facility repair and improvement and component checkout for the High Performance Demonstration Experiment have been completed and new high powered tests have been conducted during FY81. In the Faraday configuration, 30.5 MW of power has been produced at a magnet field strength of 3.1 T for an enthalpy extraction of about 11 percent. The distribution of Faraday voltage, current, and power is uniform along the channel and is in good agreement with theoretical predictions. A Faraday voltage of about 1,650 v has been developed over much of the channel and a Faraday current and power of 80 amp and 90 kw have been sustained on numerous electrode pairs. The magnet has been operated up to 4.0 T or about 70 percent of its design value. The distribution of the field in the bore is in good agreement with design predictions. The voltage drop in the "hot" plasma layers over the electrode wall (pyrolytic graphite caps) is significantly reduced from that previously observed on the "cold" plasma layers (ATJ graphite caps). The average magnitude of the presently observed voltage drop is 60 percent of the "cold" wall voltage drop. Power production and enthalpy extraction calculations, which utilize the new "hot wall" voltage drop, indicate that the overall objectives of the HPDE (15 percent enthalpy extraction) are still achievable. A Hall voltage transient has been observed during recent tests. However, the magnitude of the voltage transient has been significantly reduced by introducing the seed and fuel simultaneously.

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NOMENCLATURE

B	Magnetic field, Tesla
$B_{\max}$	Maximum magnetic field, Tesla
E_x	Electric field, Flow Direction, v/m
E_y	Electric Field, Across Channel, v/m
H_I	Enthalpy at Channel Inlet, Joule/kg
I	Current, amp
J	Current density, amp/m ²
J_y	Current density, Across Channel, amp/m ²
$\dot{m}$	Mass flow rate, kg/sec
p	Pressure, bar
P_0	Burner pressure, bar
R_L	Load resistance x electrode pitch
T_f	Flame temperature, °K
T_w	Wall temperature, °K
T_1	Time measured from ignitor pressure, >30 psi
T_2	Time measured from Fuel Valve 5-3 leaves closed position
U_I	Velocity, channel entrance, m/sec
u	Velocity, m/sec
V_D	Voltage drop, v
V_{DA}	Voltage drop anode, v
V_{DC}	Voltage drop cathode, v
V_{oc}	Power supply open circuit voltage, v
ΔH	Amount of enthalpy extracted, Joule/kg
σ	Conductivity, S/m
σ_I	Conductivity at channel inlet, S/m
$\omega\tau$	Hall parameter

APPENDIX A

DEFINITION OF PERFORMANCE PARAMETERS

A.1 INTRODUCTION

The overall objective of the High Performance Demonstration Experiment is to show that an MHD generator channel (of sufficiently large scale) will perform at an efficiency level high enough to make its application to commercial power generation a viable energy alternative. The purpose of this appendix is to define the performance parameters utilized in the HPDE project; to discuss the methods used in their calculations, and to present the assumptions upon which those calculations are based.

A.2 DEFINITION OF PERFORMANCE PARAMETERS

There are numerous parameters which are routinely used as indicators of generator channel performance. Perhaps the most basic parameter is the amount of power which a given channel produces when operating at its design conditions, but that performance indicator serves only to establish the nominal power level under investigation and provides no clue as to how efficiently the power was produced. A second parameter, denoted as power density, provides additional information about relative channel performance. It is defined as the power produced per unit channel volume and serves as a general indicator of the physical size involved for particular power levels. In general, a high power density indicates an emphasis on system compactness rather than an emphasis on system efficiency. The specific energy extraction - defined as the energy extracted per unit mass flow - is also often quoted as a measure of channel performance. That parameter not only gives an indication of the physical size for a particular power level, but also provides a rough indication of the efficiency which might be expected.

The above parameters are important indicators, but when considering commercial application of the MHD process, the primary channel performance parameter is the enthalpy extraction ratio. It is defined as the power extracted by the channel divided by the enthalpy available at the channel inlet. In equation form

$$\eta = \frac{H_I - H_F}{H_I} \quad (A-1)$$

A secondary performance parameter, the turbine efficiency, is defined as the actual work output divided by the isentropic work

$$\eta_t = \frac{\text{actual work}}{\text{isentropic work}} = \frac{H_I - H_F (\text{actual})}{H_I - H_F (\text{isentropic})} \quad (A-2)$$

The initial states are identical, but the isentropic work involves an isentropic expansion to the same final pressure as the actual case.

The turbine efficiency is then a measure of how well the MHD generator removes energy from the flow as compared to an ideal isentropic process operating between the same inlet and outlet conditions.

In order to calculate the overall channel efficiency and the turbine efficiency, the channel inlet enthalpy must be accurately determined. The methods used to calculate the inlet enthalpy and the assumptions upon which those calculations are based are presented in the following section.

A.3 CALCULATION OF TOTAL ENTHALPY

The specific chemistry of the combustion reaction is a necessary factor in the enthalpy determination. To approximate the C/H ratio of coal in the HPDE experiment and yet utilize a "clean" fuel, the decision was made to use toluene as the primary fuel. The oxidizer used is a synthesized mixture of oxygen and nitrogen. The seed material used is potassium hydroxide which is suspended in methanol.

Since the experiment is currently being conducted with a fuel-rich mixture, incomplete combustion occurs. It is assumed that the incomplete combustion results in the formation of CO in the products and that there are no unburned C or H atoms. The chemical reaction for the fuel-rich case then becomes:

By chemically balancing the above equation, it is possible to determine the amount of CO produced.

It is, however, necessary to establish an enthalpy reference level and the energy release during combustion in order to establish an enthalpy level after combustion.

The zero enthalpy reference level for the HPDE is taken at 0°K. The reference level is significant when a performance parameter such as the percent enthalpy extraction is being calculated. The lower heating value, LHV, is used in calculations involving the heat release during combustion. With these definitions, the sensible energy carried by the reactants as they enter the burner is one contribution to the input enthalpy. For a typical run, this may account for some 7 percent of the total.

The amount of energy released during combustion can be calculated by one of two methods. One way is to use the heat of combustion of the

individual elements. If this is done, the heats of formation of the reactant compounds must be subtracted. An alternate approach is to use the heats of combustion of the reactant compounds. In either case, the heat of formation of potassium hydroxide, KOH, must be subtracted since the energy required to break up the seed compound must come from the fuel.

While comparing the results from these two methods, it was discovered that inconsistencies exist in the published values of heats of combustion and formation. The following is an internally consistent set of constants that is believed to be the most reliable of those available. They have been compiled from the Eighth Edition of Marks Handbook and the Forty-Seventh Edition of the CRC Handbook.

Table A-1

	HHV joules/kg	LHV joules/kg	Heat of Formation joules/kg
C to CO ₂	32.79x10 ⁶		
C to CO	9.2 x10 ⁶		
CO to CO ₂	10.1 x10 ⁶		
H ₂ to H ₂ O	141.98x10 ⁶	120.0 x10 ⁶	
Toluene, C ₇ H ₈	42.9 x10 ⁶	40.94x10 ⁶	-0.55x10 ⁶ (vapor)
Methanol, CH ₃ OH	22.7 x10 ⁶	19.94x10 ⁶	7.51x10 ⁶ (liquid)
Potassium Hydroxide, KOH			8.6 x10 ⁶

In the one-dimensional theoretical channel performance prediction program used at AEDC, the enthalpy is calculated assuming the products of combustion to be formed from their elemental constituents and then using the heats of formation of the elements. The data reduction program, on the other hand, uses the heats of combustion of the reactant compounds. The two methods yield the same enthalpy levels provided the consistent set of values listed in Table A-1 are used. Following the data reduction program, the total enthalpy after combustion is given by:

$$H_{\text{Total}} = H_{\text{Fuel}} + H_{\text{seed}} + H_{\text{oxidizer}} - H_{\text{CO}} - H_{\text{KOH}} \quad (\text{A-3})$$

The sensible enthalpy before combustion is included in the total energy released during combustion. Lower heating values from Table A-1 are used. If combustion is not completed, the energy tied up as CO is subtracted. Also subtracted is the energy required to break up the KOH. Each of the terms in Eq. (A-3) can be expressed as follows, where flows are in kg/sec, temperatures in °K, and enthalpies are in watts.

$$H_{\text{fuel}} = F_{\text{fuel}} \left[40.94 \times 10^6 + 1616 T_{\text{fuel}} \right] \quad (\text{A-4})$$

$$H_{\text{seed}} = \frac{F_{\text{seed}}}{1 + \text{SAT}} \left[19.94 \times 10^6 + 2370 T_{\text{seed}} \right] \quad (\text{A-5})$$

$$H_{\text{oxid}} = 1002.8 F_{\text{oxid}} T_{\text{oxid}} \quad (\text{A-6})$$

$$H_{\text{CO}} = 10.1 \times 10^6 \left[5.47 F_{\text{fuel}} - \frac{F_{\text{oxid}}}{0.571 + 0.5 N_2 O_2} + F_{\text{seed}} \left(\frac{2.62 - 0.25 \text{ SAT}}{1 + \text{SAT}} \right) \right] \quad (\text{A-7})$$

$$H_{\text{KOH}} = 8.6 \times 10^6 F_{\text{seed}} \left(\frac{\text{SAT}}{1 + \text{SAT}} \right) \quad (\text{A-8})$$

Equations (A-3) through (A-8) can be solved to determine the total enthalpy of products of combustion after all reactions have proceeded to completion. Assuming no heat loss in the combustor or in the inactive entrance region of the channel, the enthalpy determined from Eq. (A-3) would also be the enthalpy at the first loaded electrode, and would be the enthalpy used to calculate efficiencies in Eqs. (A-1) and (A-2). Such losses do occur, however, and those losses must be considered if an accurate calculation of channel efficiency is undertaken. It should be noted that the HPDE is designed to demonstrate channel performance and not overall system performance.

In order to solve Eqs. (A-3) through (A-8) and thus determine the total enthalpy in the burner, a number of crucial measurements are required. These measurements include the flow rates and initial temperatures of the combustor inputs. The fuel and seed flows are measured with vane-type flowmeters and the oxidizer flow is measured with an unchoked venturi. The nitrogen-oxygen ratio in the oxidizer and the potassium hydroxide-methanol ratio in the seed mixture are determined by laboratory analysis. The temperatures of all the components are measured with thermocouples in their respective flow lines. The measurement of the above quantities are substituted into Eqs. (A-4) through (A-8) and finally, the total enthalpy is obtained from Eq. (A-3).

The total enthalpy at the channel inlet is reduced from the combustor enthalpy by the amount of the combustor losses. The burner, burner backplate, and nozzle are each water-cooled in order to maintain structural integrity. The flow and the temperature rise of the cooling water are measured during the test. The magnitude of the cooling water losses is typically 7.5 percent of the combustor enthalpy.

Actual calculation of the channel efficiency is quite straightforward, of course, once the prevailing assumptions and definitions have been clearly established. The actual power extracted in the HPDE channel in a given run is determined by a simple summation of the products of the Faraday voltages and currents. The channel inlet enthalpy for that run is then calculated as described above and the channel efficiency during that run is determined. The enthalpy extraction ratios (efficiency) achieved in the HPDE to date are shown in Fig. 32 of the main text.

A.5 UNCERTAINTY ASSOCIATED WITH THE PERFORMANCE PARAMETERS

Each of the measurements taken during the experiment has an uncertainty associated with it. In performing an analysis of these uncertainties, it is easier to determine an uncertainty for each of the items of data taken than to make a direct estimate of the uncertainty

of the final result. The error associated with the primary measurements can then be propagated through the data reduction equations to arrive at the uncertainty of the final result.

If the uncertainties in the basic measurements of flows, temperatures concentrations, currents, and voltages are propagated through the data reduction equations, the following expression is obtained for the uncertainty associated with the enthalpy extraction.

$$E_{\text{enthalpy extraction}} = \left[0.1(E_{F_{\text{fuel}}})^2 + 0.003(E_{F_{\text{seed}}})^2 + 1.85(E_{F_{\text{oxid}}})^2 + 0.34(E_{N_2O_2})^2 + 5.6 \times 10^{-7}(E_{SAT})^2 + 0.0036(E_{\text{cooling flow}})^2 + 0.0036(E_{\text{cooling } \Delta T})^2 + 0.13(E_{\text{volt}})^2 + 0.13(E_{\text{cur}})^2 \right]^{1/2} \quad (A-9)$$

An examination of the coefficients of the various terms reveals that the oxidizer flow and the nitrogen-oxygen ratio are the measurements most important for an accurate determination of the enthalpy extraction.

The uncertainties associated with each of the basic measurements made are as follows:

Fuel flow	±1 percent
Seed mixture flow	±1 percent
Oxidizer flow	≤ ±2 percent
Nitrogen-oxygen ratio	±1 percent
Seed concentration	±0.1 percent
Temperature	±1°F
Cooling water flow	±2 percent
Cooling water ΔT	±0.5°F
Electrical current	±1 percent
Voltage	±0.1 percent

Using these values in Eq. (A-9) results in the following uncertainties:

Power extraction	± 0.1 percent
Enthalpy level at channel inlet	± 2.8 percent
Enthalpy extraction level	± 2.8 percent

(NOTE: A nominal enthalpy extraction of 15 percent implies an uncertainty range of 14.6 to 15.4 percent.)

A.6 SUMMARY

The three most important performance parameters for an MHD generator are the power extracted, the percent enthalpy extraction, and the turbine (or isentropic) efficiency. The power extracted involves the measurement of current and voltage. The percent enthalpy extraction is defined as the power extracted divided by the enthalpy level at the entrance to the channel. It is important to note that the zero level for enthalpy is taken at 0°K. The lower heating value is used to evaluate the energy released during combustion. Finally, the turbine efficiency is defined as the ratio of the actual enthalpy extracted in the channel to the enthalpy change in an isentropic expansion between the same pressures.