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REPORT ON THE MHD PERFORMANCE DEMONSTRATION
EXPERIMENT FOR THE PERIOD OCTOBER 1, 1977 TO
SEPTEMBER 30, 1978

By
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MASTER

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By

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ABSTRACT

The Arnold Engineering Development Center (AEDC) has been under contract since December 1973 to modify existing equipment and to design, fabricate, and install new hardware to perform an MHD Performance Demonstration Experiment. The objective of the experiment is to demonstrate that a generator simulating a commercial-sized device can convert 16 to 18 percent of the available thermal energy into electrical power. This report describes fabrication, installation, and testing of hardware for this experiment during the period from October 1, 1977, to September 30, 1978. In the past year, fabrication of the high performance generator channel was completed, and satisfactorily pressure and electrically checked. The coils and approximately 95 percent of the coil force containment structure were installed on the 6 Tesla magnet. Fabrication of the outer insulation panels for the magnet was completed.

The magnet is to be cryogenically cooled with liquid nitrogen. During the past year, analytical studies to provide guidance in performing the cooldown were completed and are presented. The preliminary design of the cooling manifold system to provide the required control in accordance with the results of the analytical studies was also completed and is reported.

Additional testing of the burner system with the diagnostic section installed was conducted and the results for seeded and unseeded operation are included.

I. INTRODUCTION

Commercial viability of magnetohydrodynamic (MHD) power generation in a coal-fired baseload plant requires that the MHD generator have an energy extraction approaching 0.20 with a turbine efficiency of 70 percent in order to achieve an overall plant efficiency of 50 percent. This required performance represents a substantial increase over that which has been demonstrated to date. Since the commercial MHD concept is critically dependent upon the generator performance, it is essential that such performance be demonstrated on a scale which is economically acceptable yet of sufficient size to permit scaling to projected commercial size with reasonable confidence.

The Arnold Engineering Development Center (AEDC) has been under contract to the Office of Coal Research, the Fossil Fuel Division of the Energy Research and Development Administration (ERDA) and subsequently to the Department of Energy (DOE) since December 1973 to perform such a demonstration, referred to as the High Performance Demonstration Experiment (HPDE). The experiment will be performed with a 6 Tesla (T) magnet, subsonic generator, and existing hardware in the AEDC High Temperature Laboratory (HTL) (Ref. 1). Fuel and oxygen combustion gases will be used which simulate the hydrogen-carbon ratio of coal without the slag and ash impurities usually found in coal combustion products. The HTL was previously used for MHD research on the USAF LORHO project (1962 to 1969) and many of the system components associated with the consumables and utilities remained in existence at the initiation of the current project. During the first 18 months of the contract, the ancillary system components for the experiment were removed, rejuvenated, or replaced, as appropriate. An automated data acquisition system was also installed. Major new components, including the magnet, generator channel, diffuser, and load bank, were also designed in the first year and a half of the contract. The renovations and design of new hardware are presented in the report covering the period November 15, 1973, to April 30, 1975 (Ref. 1).

Following the completion of the detailed design phases of the major components, the next sixteen months of the contract were largely devoted to procurement of materials, fabrication of components, and assembly. In addition, diagnostic instrumentation for measurement of the plasma characteristics was designed and fabricated. The report for this period (Ref. 2) describes the instrumentation development in addition to elucidating problems encountered with component fabrication for the generator channel and magnet which resulted from the use of nonconventional materials in sizes commensurate with an MHD system of this magnitude.

The next 12-month effort, from October 1976 to September 1977, was devoted to shakedown testing of the burner, continued production of the generator channel parts, and production and testing of coil pancakes for the magnet. Assembly operations on the channel and magnet were started.

One section of the channel was completed and the first coil pancake was installed on the magnet lower pole. Activities for this period are summarized in Ref. 3.

The present report summarizes activities performed on the MHD HPDE in the period from October 1, 1977, to September 30, 1978. During this year, additional burner testing with various flow diagnostics was performed. Assembly and testing of the five generator channel sections were completed and, at the close of the period, alignment and trial connection of the sections into a single unit were started. The magnet assembly was continued with all coils installed and approximately 95 percent of the force containment structure along with its associated structural instrumentation was installed. Analytical studies of the magnet cooldown process and the design of liquid nitrogen (LN₂) manifold-ing and control systems for cooling were also completed. The fabrication of the vapor box for the saddle crossovers was completed. Difficulties with the vacuum box liner for the magnet bore have raised serious doubt as to whether the structure could ever achieve its design performance and an alternate styrofoam liner concept is being investigated.

II. GENERATOR SYSTEMS

2.1 GENERATOR CHANNEL

Assembly operations on the generator channel were started in FY77 and one of the five sections was completed. During the current reporting period, the assembly of the remaining four sections was completed. A photograph of the completed channel sections located in the storage area is given in Fig. 1. As indicated in the photograph, preliminary alignment and connection of the channel sections on the previously completed channel cart have been started.

The assembly sequence for the channel section is fully described in Ref. 3 and will not be repeated here. However, during the course of the assembly and testing, two significant problems arose. The first problem involved unexpectedly low interelectrode resistances and the second problem of concern was flange gasket leaks encountered on channel Sections I and II.

Upon completion of assembly of channel Section IV, the first completed section, electrical testing revealed lower than anticipated interelectrode resistances, generally in the range from 1 k Ω to 10 k Ω . It appeared that the low resistances in Section IV were due to residual moisture in the refractory, even though the refractory had several weeks to dry in the ambient air. Additional forced drying was performed by capping the ends of the section and maintaining an internal vacuum of 103 Torr with a heating lamp inside. Removal of significant moisture was evidenced in the vacuum pump oil and a significant improvement in the interelectrode resistances was observed.

Figure 1. Photograph of Completed Generator Channel Sections and Channel Cart

At this time, the assembly of channel Section V was completed and initial electrical testing revealed inlet-to-exit shorting of both electrode walls. This was even more unexpected since the interpeg resistances in the B-walls of channel Section V, as well as Section IV, were of the order of 500 k Ω .

Section V was partially disassembled to determine the source of the extremely low interelectrode resistances. Several surprises were eventually found, notably that the rigidizer used for the Kaowool[®] corner insulation, as well as the phosphate-based binder in the KR-98 refractory chemically attacked the copper when wet. Diffusion of copper ions through the ceramic and Kaowool resulted and any subsequent exposure to moisture, after having been dried once, resulted in a lower volume resistivity.

In an attempt to eliminate the problems encountered with the phosphate-based binder, evaluations of alternate refractories to substitute for the KR Bond 98A were conducted. Samples of refractories tested included: Fiberflax QF-180[®], Ceramacast 510[®], Saureisen No. 8[®], Saureisen No. 31[®], and Saureisen No. 75[®]. All of these refractories were of lower indicated service temperature than the KR Bond 98A. Tests were performed using actual electrodes removed from channel Section V and none of the refractories attacked the copper. The Saureisen No. 75 appeared superior with regard to electrical resistance in that it achieved a high resistance and dried faster than any of the others. However, the Saureisen No. 75 bonded poorly at best to the electrodes and graphite caps.

Concurrent with the evaluation of possible alternate refractories, additional electrical resistance measurements were made on channel Section IV, which had been dried under vacuum, to determine the effect of daily humidity variations on the insulating property of the KR Bond 98A refractory. To standardize the measurements and minimize the effect of decomposition potential, a leakage current measuring unit was built. This device was essentially a multirange milliammeter incorporating a voltage-regulated power supply which delivered 32 v to approximate the channel design value of interelectrode voltage.

In Tables 1 and 2, sample measurements of the resistance between adjacent electrodes on the two electrode walls of Section IV are given. The wall designations right and left are referenced by viewing downstream at the inlet end. Measurements were taken eight times over an 18-day period during which the channel section was exposed to indoor humidity typical of a large industrial shop area. The interelectrode gaps monitored and listed in Tables 1 and 2 represent 13 percent of the 246 total interelectrode gaps in channel Section IV. In general, the measured resistances of the right electrode wall were lower than the left wall. In both walls, the interelectrode resistances tended to be higher at the inlet end than in the center and toward the exit end. This variation is thought to be due to deeper interelectrode insulating

Table 1

Interelectrode Resistance Measurements of Channel
Section IV Right Wall in Kilohms

Electrode Pair	Nov 11	Nov 14	Nov 15	Nov 16	Nov 17	Nov 21	Nov 23	Nov 29	Mean	rms Dev.
267-268	-	27	41	68	67	67	84	213	81	56
308-309	20	46	52	19	14	20	21	57	31	16
311-312	800	213	228	110	84	84	97	246	232	224
314-315	160	355	320	152	133	114	152	533	240	139
323-324	10	32	29	19	13	17	19	38	22	9
327-328	6	23	18	6	4	5	4	8	9	7
333-334	8	15	15	10	7	10	11	33	14	8
341-342	5	9	9	7	5	6	5	10	7	2
343-344	-	-	13	9	7	9	8	18	11	4
351-352	5	10	10	7	5	6	6	11	7	2
356-357	15	34	38	23	15	17	16	34	24	9
360-361	4	9	9	7	5	6	5	9	7	2
381-382	8	20	21	15	10	11	10	21	14	5
383-384	46	133	145	71	32	46	46	145	83	46
384-385	47	57	59	48	50	42	44	52	50	6

Table 2

Interelectrode Resistance Measurements of Channel
Section IV Left Wall in Kilohms

Electrode Pair	Nov 11	Nov 14	Nov 15	Nov 16	Nov 17	Nov 21	Nov 23	Nov 29	Mean	rms Dev.
263-264	12	13	13	9	9	9	8	10	10	2
266-267	228	533	533	356	356	320	320	533	397	111
267-268	800	533	533	400	400	356	400	533	494	134
269-270	103	128	128	110	107	356	100	114	144	81
270-271	800	457	457	320	320	266	290	457	421	162
278-279	114	128	128	123	118	114	114	123	120	6
295-296	100	533	457	291	246	246	400	400	334	130
321-322	10	11	11	9	9	9	9	9	10	1
325-326	16	39	46	40	34	45	39	62	40	12
329-330	13	55	61	188	43	82	94	228	95	69
352-353	12	57	65	52	42	86	106	266	86	73
359-360	16	67	78	59	43	65	73	178	72	44
360-361	50	266	320	228	188	213	229	400	236	95
381-382	2	13	8	6	5	4	4	5	6	3
382-383	41	267	320	188	133	178	200	457	223	118
383-384	640	1067	800	640	800	640	800	800	773	133
384-385	800	1067	1067	800	1067	457	800	800	857	195

gaps and widening of the electrodes toward the section exit which results in increased area of contact between the electrodes and refractory. This conjecture is also compatible with the observed high interpeg resistances since the contact area of the individual pegs with the refractory is much smaller than in the case of the electrodes and the depth of the refractory between the pegs is less as well, which would promote faster and more thorough drying.

The results in Tables 1 and 2 exhibit considerable variation with respect to time and there is no obvious time correlation for the variations observed between measurements made on the same wall or between measurements made at the same locations on the opposite walls, where the geometries would be similar. However, the results indicate that for individual refractory gaps the interelectrode resistances appear stable in that they do not systematically increase or decrease in time. The largest variations occur where the average resistance is high and vice versa,

From the standpoint of minimum tolerable resistance between adjacent electrodes, a value of 1 k Ω would appear to be acceptable. Under design conditions such a resistance would result in an interelectrode leakage current of only 0.1 percent of the electrode load current. From the measurements made, it was concluded that the KR Bond 98A refractory is capable of being dried to an acceptable level and that environmental humidity will not significantly degrade its usefulness as an electrical insulator to the point where it must be redried.

A second question investigated concerned the recovery, or drying, of the KR Bond 98A refractory once it had been wetted, for example, in the event of an upstream coolant passage failure in the combustor or diagnostics section. For this test, three of the electrode gaps in channel Section IV were wetted and the interelectrode resistance was monitored for several days afterward while the channel dried in ambient room conditions. The resulting measurements for this test are shown in Table 3. From this table, it appears that the recovery time is approximately 36 hours.

Based on these tests and lacking an obviously superior alternative refractory, it was decided to complete the channel with the KR Bond 98A, eliminate the use of Kaowool rigidizer, and force-dry the refractory as soon as practical after it was poured.

Upon completion of each channel section, pressure tests were performed to verify the integrity of the seals. Channel Sections I and II are designed to operate with the highest plasma pressure and correspondingly were tested to higher pressures than the other three sections which were completed earlier. Pressure testing of channel Section II revealed leaks in the flange corners which first appeared at approximately 40 psig and remained up to the design test pressure of 50 psig.

Table 3

Interelectrode Resistance Measurements after Wetting
and During Drying in Kilohms

Date	Condition	Electrode Pair Resistances		
		268-269	286-287	366-367
Nov. 11	Dry	800.0	12.0	5.2
Nov. 14	Dry	180.0	16.0	11.8
Nov. 14	Soaked	0.07	0.03	0.03
Nov. 15 AM	Drying	6.0	0.80	1.00
Nov. 15 PM	Drying	41.0	0.91	0.91
Nov. 16 AM	Drying	32.0	2.1	2.1
Nov. 16 PM	Drying	26.7	3.2	2.6
Nov. 17 AM	Drying	32.0	3.2	4.0
Nov. 17 PM	Drying	800.0	1.6	4.0
Nov. 21	Drying	37.6	7.3	5.6
Nov. 23	Drying	58.2	6.0	5.4
Nov. 29	Drying	168.0	13.3	11.4

The relevant geometry of the flange gasket is depicted in Fig. 2. Each corner where the insulating and electrode walls meet is sealed with a continuous longitudinal O-ring. When the walls are assembled, GE RTV silicon sealant is also embedded in the O-ring groove for a distance of approximately 2.5 cm at the flange end of the groove. After the walls have been secured together, the excess longitudinal O-ring is trimmed flush with the flange face. The flange seal is comprised of a fiberglass frame (NEMA Grade G-7) which has two circumferential O-ring grooves. The circumferential O-ring, in general, passes to the inside of the exposed end of the longitudinal O-ring. With this arrangement, there are two plausible sources for the leaks observed. First, the flange O-ring invariably crosses a cured RTV sealed joint, either between the walls or the end of the longitudinal O-ring groove. In either instance, the RTV would represent a pliable sealing surface which is not conventional practice, but unavoidable in this instance. Secondly, the sealing O-rings in all instances are of a silicon rubber material and the bonding with the RTV is poor. In view of the poor bond, it is possible that the gas leak comes from the inside of the longitudinal O-ring groove, following the O-ring through the RTV at the end of the groove, and exiting to the outside of the flange gasket O-ring.

Figure 2. Detail of Channel Flange Gaskets and Seal Geometry

It was demonstrated on channel Section II that a simple, flat rubber gasket would eliminate the leaks. The upstream flange gasket for Section II was modified by milling both sides such that two flat gaskets of the same thickness as the O-ring could be installed in place of the O-ring. This gasket was used to pressure-test channel Section I up to its design pressure of 60 psig with satisfactory results. It is anticipated that this modification will be required for the two flange gaskets of channel Section II only.

2.2 CHANNEL INSTRUMENTATION

The major portion of channel instrumentation has been presented in Refs. 2 and 3 and will not be repeated here. However, the instrumentation for measurement of power extraction is pertinent to the overall objectives and has not been previously addressed in detail.

As an overview, Table 4 lists all instrumentation presently planned for a typical powered run. At present, 575 data channels are planned, which is more than double the 256 channels identified at the project onset. The row headed "Generator Channel" denotes measurements dedicated to channel diagnostic type measurements, while the row headed "Load Bank" contains measurements dedicated to determining the channel power output and electrical characteristics. The diffuser instrumentation also gives an indication of the channel performance, of course, as well as providing the necessary information for determining the optimum wall angle for efficient diffuser operation. The diffuser design and instrumentation have also been previously reported (Ref. 2). The magnet instrumentation is discussed in Section III, but is included in Table 4 for completeness.

The type and location of measurements to be taken on the electrode walls are shown in Fig. 3. The scope of instrumentation presently incorporated is inadequate to obtain detailed data and is intended only to provide sufficient data to evaluate overall channel performance with reasonable confidence.

To perform electrical measurements on the channel, isolation of the instrumentation with respect to the Hall voltage is a significant problem. For the HPDE, the vast majority of the electrode pair current and voltage measurements will be made with banks of panel meters using photographic recording. While this scheme is conceptually simple and relatively inexpensive, a penalty is paid in that the turnaround time for the reduced data is greater than desirable. Provision has been made to monitor eleven electrode voltages and eleven electrode currents with the data acquisition system to provide limited "quick look" data and cross check the panel meter measurements. Table 5 presents a tabular list of electrode measurements to be made along with the expected values and the scheme to be used. The panel meters for the cathode electrodes will measure the Hall voltage across groups of approximately ten electrodes. At every point the Hall voltage is measured, the Faraday voltage (cathode to anode) will also be measured along with the current

Table 4

Channel Power Run Instrumentation

Component	Data Channels	
	Planned - 1977	Planned - 1978
A. GENERATOR CHANNEL		
1) Temperature	3	10
2) Static Pressures (Dyn. Press.)	29	40 (1)
3) Voltages, Currents	23	23
4) Pegwall Voltage		28
B. LOAD BANK		
1) Water Flow	1	1
2) Water Inlet Temperature	1	1
3) Water Outlet Temperature	1	1
4) Voltages	80	82
5) Currents	40	41
6) Camera Trigger and Timing Circuits		2
C. DIFFUSER		
1) Wall Temperature	2	2
2) Static Pressures	15	15
3) Total Pressure Probe	1	1
4) Probe Position	1	1
5) Pressure Rake at Exit	3	3
D. MAGNET		
1) Current	1	1
2) Voltage	1	1
3) Temperature	8	110
4) Strain Gages	6	128
5) Field Strength Probes		20
E. OTHER		
1) Pressure Reference	8	8
TOTAL	224	520
PLUS BURNER INSTRUMENTATION	32	55
	256 (4)	575 (3)

NOTES:

- ① Static pressures have been added to channel to assess performance.
- ② Realistic consideration of magnet cooldown and complete analysis of FCS loads dictate more extensive measurements of temperatures and strains in the magnet system to certify cyclic life and to prevent coil buckling or FCS overstress.
- ③ Of these, 190 must be on the data system (PDP 11/45), hence work is proceeding toward 160 to 180 data channels dedicated to HPDE with some channel swapout based on run-to-run priority.
- ④ Of these, about 130 would have been on data system.

- Anode to Ground, volts
- △ Cathode to Ground, volts
- ◇ Cathode to Cathode, volts
- ◆ Cathode to Anode, volts
- Current (I), amps

Open Symbol - Panel

Solid Symbol - Computer

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Figure 3. Electrode Wall Measurements

Table 5
Electrode Wall Measurements

1	2	3	4	5	6	7
Electrode Number	Reference Station Number, In.	Computer Cathode to Ground, V	Computer Anode to Ground, V	Panel Cathode to Cathode (Meter Range)	Panel Cathode to Anode Range, V	Panel (Computer) Current (I) Cathode To Anode amp
0	0.0	100		Gnd (50)	100	100 Computer
10	10.63			0 (50)	450	
20	21.25			10 (50)	750	
30	31.25			20 (50)	1000	
42	39.62		1500	30 (100)	1200	C
52	47.57			42 (150)	1300	100
62	55.56			52 (275)	1400	
72	62.44			62 (200)	1500	
84	70.69	1200		72 (300)	1400	C
94	77.56			84 (250)	1400	
104	84.44			94 (200)	1550	100
114	91.31			104 (200)	1600	
126	99.56		3750	114 (300)	1600	C
136	106.44			126 (200)	1650	
146	112.99			136 (250)	1700	
156	119.87			146 (250)	1700	100
168	127.56	3100		156 (250)	1750	C
178	133.18			168 (200)	1800	
188	138.81			178 (200)	1800	
198	144.43			188 (250)	1750	
210	151.18		5900	198 (200)	1850	100 C
220	156.81			210 (250)	1950	
230	162.43			220 (200)	2000	
240	168.06			230 (300)	2000	
252	174.81	4900		240 (300)	2050	C
262	180.62			252 (200)	2000	100
272	186.41			262 (250)	2200	
282	192.03			272 (250)	2200	
294	198.78		8150	282 (250)	2300	C
304	204.65			294 (250)	2350	
314	210.28			304 (300)	2350	100
324	215.90			314 (250)	2400	
336	222.65	6950		324 (300)	2450	C
346	228.28			336 (300)	2600	
356	233.90			346 (250)	2500	
366	239.53			356 (300)	2500	100
376	245.15		10,550	366 (300)	2400	
386	251.03			376 (350)	2300	
396	259.06			386 (450)	2100	
406	269.39			396 (600)	1800	
417	281.38	10,050		406 (600)	1350	100 C
Diffuser	319+	10,000				

in this particular pair. The cathode and anode voltage distribution relative to ground can then be determined by adding appropriate meter readings. The electrode voltages monitored with the data acquisition system will be measured with respect to ground. As indicated in Table 5, a measurement of an electrode voltage to ground will be made at approximately every fortieth electrode and alternating between cathode and anode electrodes. At every electrode-to-ground measurement point, the current flow in the electrode will also be monitored by the data acquisition system.

The current measurements made with the data acquisition system will be made with commercially available differential transformer devices which provide adequate isolation for the Hall voltage. The corresponding voltage measurement will be performed with a resistive divider network. The resistive dividers were fabricated at AEDC and are complete.

A block diagram of all HPDE instrumentation and data acquisition systems is shown in Fig. 4. At present, all meters required for the meter panel are on hand and the layout for that system is complete. Installation of the meter panel has been scheduled for early FY79. Additional spare voltage towers are presently being fabricated. All pressure transducers and thermocouples are on hand.

III. MAGNET SYSTEM

3.1 GENERAL STATUS

During the past year, approximately 80 percent of the magnet was assembled. A photograph of the magnet assembly at the start of the present reporting period is given in Fig. 5 and shows the lower pole pieces, the thermal insulation and the first lower coil pancake installed. A photograph of the magnet assembly near the close of the present reporting period is given in Fig. 6. During the year, all of the coils were installed and all of the force containment structure (FCS) was installed except for the four sideplates and the upper inlet end tongue. While not clearly evidenced, considerable instrumentation, 128 strain gages and 110 thermocouples, were installed on the FCS during its assembly.

3.2 COIL INSTALLATION

After the first lower coil pancake had been installed, the installation of the remaining lower pancakes became fairly routine, but because of the delicate nature of the coils, it was time-consuming.

Figure 4. Block Diagram of HPDE Instrumentation and Data Acquisition Systems

Figure 5. First Coil Installed

Figure 6. Photograph of the Magnet Assembly

The initial coil placement required special effort with regard to its alignment, as it served as a baseline for the placement of the remainder of the coils. At the initiation of the coil assembly, all of the coil pancakes had been received from the vendor, ICI in Middleton, Massachusetts. The coils were nested on storage tables, three to a table, with the shortest coil always on the bottom. Since, for the lower coil assembly, the installation proceeds with increasingly lengthened coils, considerable swapping of coils between the storage tables was required. A special coil handling fixture, as shown in Fig. 7, had been built to provide for the lifting and transporting of the coil pancakes. This fixture provided the necessary distributive support to minimize in-plane bowing of the long straight sections of the coil.

An additional complication during preparation for the installation of the coil pancakes was the straightening of the coolant tubes. These tubes were bent 90 deg in order to comply with ICC highway width requirements for shipment to AEDC. The tubes were annealed and straightened, an insulating sleeve placed over each one, and the ensemble tied together with a suitable temporary clamp prior to installation. The coils were transported from the storage area to the assembly area with a "lowboy" semitrailer on which a support table was mounted. The coil pancakes were lifted off the truck and moved to the magnet with a pair of portable gantries, as shown in Fig. 8.

Prior to placement of a coil pancake on the magnet, a final visual and electrical check of the insulation was made, with repairs and re-testing performed if necessary. The coil pancake was then lowered onto stacks of shims placed on the previously installed coil pancake. The shims provided the necessary temporary clearance to remove the transport fixture from the coil. The shims were made of thin (0.32 cm thick) steel plate and were Teflon-coated for easy removal. A special hydraulic shim puller was built to facilitate the removal of the shims and effect the final coil placement. During final placement, coil separators (saddle fillers) were installed at the inlet end. These spacers, shown in Fig. 9, were made of aluminum plate with covering sheets of Delrin[®] bonded to the two faces. The inlet saddle dimension of the coil buildup was controlled and the length tolerances of the coil pancakes were all allowed to accumulate at the outlet end. As a consequence, the spacers at the exit had to be field-fabricated to accommodate the varying space between successive saddle coil crossovers.

A problem surfaced regarding the coil radius filler blocks. These blocks are airfoil-shaped pieces that are intended to fill the inter-pancake voids at the junction of the coil horizontal runs and saddle section, as shown in Fig. 9. The shape of the radius fillers precludes simple fabrication by any means other than casting.

Figure 7. Photograph Showing Special Coil Handling Fixture

Figure 8. Transport of Coil Pancake from Truck to Magnet Assembly with Portable Gantries

Figure 9. Sketch of Coil Filler Blocks

The magnet design package provided by Magnetic Engineering Associates, the magnet designer, called for the radius fillers, but made no specific recommendations other than that they have a yield stress greater than 8,000 psi and may be cast in place. Initial tests conducted in 1976 showed that a homogeneous epoxy filler was prone to catastrophic fracture at LN₂ temperature. A reformulated material comprised of a Dow Chemical DER 332[®] epoxy resin with a filler combination of glass beads and alumina exhibited a greater resistance to thermal shock. This material was brittle at room temperature and still fractured as a result of thermal shock at LN₂ temperatures as shown in Figs. 10 and 11, photographs of sample materials which have been subjected to thermal shock by submerging in LN₂. As evidenced in the photographs, the samples developed brittle fracture on a single exposure to LN₂ temperature. Generally, the fracture was in the form of minute surface fissures which could result in complete fracture in the case of thermal cycling or mechanical shock. This resin was oven-cured and stratification of the filler always occurred during the cure cycle. The second sample shown in the figures is comprised of the same Dow Chemical resin with a filler combination of woven and chopped fiberglass. Stratification or settling of the filler again was evidenced along with considerable voids. The voids were in part due to the inability to seal the mold. This sample also exhibited the brittle surface fractures on one side (Fig. 10) where there was little filler material. In contrast, the opposite side (Fig. 11) where the fiberglass filler settled was fracture-free. The third sample shown in the figures also employed a combination of woven and chopped fiberglass filler, but the resin was No. 1710[®] manufactured by REN Division of Ciba Geigy Company. This resin cures in five hours at 70°F, but requires the casting be performed in ambient conditions of less than 55°F to achieve an acceptable worklife. The No. 1710 was void-free and showed no fracture when submerged in LN₂, and was selected for production of the coil radius filler.

The radius fillers were not crucial to the installation of the lower coils and could be installed after all lower coils were in place. In fact, in the case of the outlet saddle region, where the coil length tolerances existed, the thickness of the filler was known only after the coil was installed. In the case of the upper coils, however, the radius fillers support the corner fillers and were required as each of the upper pancakes was installed. It was necessary to fabricate an additional mold to produce four fillers per day and minimize the incurred delay.

Installation of the upper coil assembly proceeded in much the same manner as the lower coil. The coil shuffling was somewhat alleviated due to the inverse progression in lengths of the coils being installed. However, since the coils were all built, shipped, and stored in the saddle crossover-down orientation, it was necessary to invert all of the upper coil pancakes. A special rig was built to accomplish this and

Figure 10. Photograph of Radius Filler Material Samples after Thermal Shock from Exposure to Liquid Nitrogen. View of Convex Outer Surface.

Figure 11. Photograph of Radius Filler Material Samples after Thermal Shock from Exposure to Liquid Nitrogen. View of Concave Inside Surface.

a photographic sequence illustrating the operation of turning the coils over is given in Fig. 12. In addition, to facilitate the alignment of the upper coils, it was decided to install the inlet end faceplate of the FCS before the first upper coil pancake was installed. A photograph made during the placement of the final coil is given in Fig. 13.

Prior to their installation, electrical acceptance tests were repeated on coil pancakes 16, 17, and 18, since the results of the original acceptance tests were marginal with regard to measured quality (Q) and the ringing test as described in the 1977 annual report. The marginality of the results was thought to be due to external influences, including the steel tables and other coil pancakes on which the three in question were stored. It was decided to retest these coils when they could be isolated from external influences to determine if the marginal results would persist.

In Table 6 below, the measured Q values are given for the two tests. The significance of the Q test is that it can be used to indicate the presence of a shorted turn. The criterion used is that the measured Q should be reduced by at least a factor of two when one of the coil turns in a coil pancake is intentionally shorted. As subsequently more turns are shorted, the Q will decrease but in significantly decreasing increments. Thus, if a pancake has a shorted turn initially, when the Q test is performed, the observed decrease should be less than one-half value when the second turn is intentionally shorted.

Table 6
Measured Values of Q

Coil Pancake Number	Q Test August 1977		Q Test March 1978	
	Open	One Turn Shorted	Open	One Turn Shorted
16	7.2	4.58	11.0	2.85
17	7.8	4.20	10.5	2.40
18	7.0	3.65	10.8	2.65

As indicated in the table, the August 1977 measurements were qualitatively correct, but failed the factor-of-two criterion which was easily satisfied during March 1978 in the absence of the other coils.

A second electrical test is the ringing test, in which the coil pancake is connected in parallel with a charged capacitor and the decay of the oscillating voltage across the combination is monitored. The

Figure 12. Photograph Sequence Illustrating the Rollover of the Upper Coil Pancakes

Figure 13. Placement of the Final Coil

effect of an intentionally shorted turn in this test is a damping of the observed voltage oscillations. A comparison of the August 1977 and March 1978 ringing test is shown in Fig. 14 for the sixteenth coil pancake with and without one turn shorted. The decay of the voltage oscillation in the case of one turn shorted is approximately the same for both tests. However, the ringing in the unshorted pancake in the March 1978 test differed remarkably from the previous tests. Similar traces are shown for coil pancakes No. 17 and 18 in Figs. 15 and 16. The test results indicate that the three coil pancakes are free of internal shorts and that the inconclusiveness of the previous acceptance tests was probably attributable to the proximity of the steel tables and other coil pancakes to the pancake being tested.

3.3 FORCE CONTAINMENT STRUCTURE

After all of the coils had been installed and electrically checked, the installation of the force containment structure (FCS) was resumed. The first members installed after coil assembly were the longitudinal tension members (LTM). Figure 17 shows the trial assembly of the LTM's to the coil. A trial assembly was required in order to field-fit the wedge filler to the outlet end. The geometry of the coils and the LTM's is depicted in Fig. 18. The LTM's align parallel to the straight runs of the coils which diverge from inlet to exit with respect to the reference centerline of the magnet bore. The outside surfaces of the coils at the saddle crossover are parallel with the magnet centerline, however. To accommodate this geometry, the LTM's were designed with a wedge-shaped relief at the inlet end and a wedge-shaped filler at the exit end. As a consequence of the accumulation of coil manufacturing tolerances at the outlet end, the wedge fillers required field fitting. The original design called for two-piece wedge fillers on either side and provided no support for them other than a frictional fit between the LTM and the coils. As indicated in the figure, loss of the frictional support could result in the filler dropping down on the underlying coolant tubes on one side and the coil electrical connections on the other side. In the as-built configuration, the wedge fillers were fabricated in one piece rather than two and bolted to the LTM for support.

Before the final installation of the LTM's was completed, the coolant channels were also modified. During the cooldown of the magnet, the control of thermal contraction of the LTM's in conjunction with that of the coils is the single most crucial operation. The original design for the cooling of the LTM is shown in Fig. 19. In the original concept, six coolant channels (three on top and three on the bottom) were to be used to cool the members. Short channels were to be used between the collars and faceplates at both ends and with single long channels between the collars. The long channel was to be fed from one of the short sections in a series-connection as shown. This concept was less than desirable in several aspects, foremost of which was that no provision

a. Acceptance Test August 1977

b. Retest March 1978

Figure 14. Capacitor Discharge Test Results for Coil Pancake 16

OPEN

ONE TURN SHORTED

a. Acceptance Test August 1977

OPEN

ONE TURN SHORTED

b. Retest March 1978

Figure 15. Capacitor Discharge Test Results for Coil Pancake 17

OPEN

ONE TURN SHORTED

a. Acceptance Test August 1977

OPEN

ONE TURN SHORTED

b. Retest March 1978

Figure 16. Capacitor Discharge Test Results for Coil Pancake 18

Figure 17. Trial Assembly of Longitudinal Tension Members to Field-Fit the Outlet End Saddle Fillers

Figure 18. Wedge Filler Blocks and Coil-LTM Geometry

Figure 19. LTM Cooling Channel Installation

for routing the required feed tube through the collar assembly was provided at either end. In addition, if a leak developed in the short channel, the possibility of losing coolant and control of the cooldown of the major part of the LTM would be present. Finally, it could be expected that the long coolant channel would cool substantially faster than the LTM, resulting in a significant difference between the contraction rates of the coolant channel and the LTM. To correct these deficiencies, the long cooling channels were divided in half and supplied with individual feed lines at their center. The feed lines were routed out between the side beam modules with additional clearance slots milled in the side beam modules.

Coolant channels on the collars and faceplates were also originally designed as being series-connected, as shown in Fig. 20. For the collars, which are made in four pieces each, individual feed lines were added in the as-built configuration. The original configuration for the coolant channels was retained on the faceplates since, in this case, the channels would be readily accessible in the event of a malfunction.

3.4 INSULATION SYSTEM

The magnet coils and FCS are to be cooled to 77°K with LN₂, while the steel yoke remains at room temperature. To provide insulation, the outside of the FCS, which completely envelops the coils, is covered with a 10-cm thick, high-density styrofoam. Within the steel yoke, an additional covering of Mylar[®] sheet over the styrofoam is used as a vapor barrier between the styrofoam and the outer steel. The end boxes of the FCS, which enclose the saddle coil crossovers, will, in turn, be enclosed by a ribbed-sheet aluminum vapor barrier with styrofoam bonded to the inside, resulting in a dead air space between the styrofoam and the FCS members. The aluminum panels for the end box vapor barriers are all fabricated and in storage. Figure 21 is a photograph of typical panel sections illustrating the nature of their construction.

In addition to its insulating properties, the styrofoam is also a structural material. For example, the entire coil and FCS weight is partially supported through the styrofoam to the lower steel yoke. There are a limited number of strategically located 10-cm square blocks of G-11 laminate embedded in the styrofoam to support the majority of the load. Similar blocks will be embedded in the styrofoam along the side and top steel yoke pieces as well. However, as a consequence of tolerances and contractive motion of the magnet during cooldown, it can be expected that some loads will be transmitted through the styrofoam as well. The styrofoam was tested for compressive stress using 10.6-cm cubical blocks as samples; the results are given in Fig. 22. The results indicated that the ultimate strength is approximately 100 psi, the manufacturer's quoted figure, and the strength is substantially greater (17 percent) while the styrofoam is chilled to 77°K.

ORIGINAL

AS BUILT

Figure 20. Collar Cooling Channel Installation

Figure 21. Photograph of Completed Inlet End Top Panel and Access Door for the HPDE Vapor Box Enclosure

Figure 22. Stress-Strain Relation for High Density Styrofoam

As originally designed by MEA, the bore of the magnet was to be insulated with a vacuum box liner. This liner is a double-walled sheet-metal structure with the outside being Invar[®] and the inside stainless steel. The walls of the structure are separated by a waffled polycarbonate sheet which prevents the walls from collapsing when a vacuum is drawn on the box. The insulating box is actually two separate boxes, one of which fits in the inlet saddle region and a much longer one which fits in the outlet saddle and extends through the magnet aperture to mate with the inlet box. In order to support the channel tracks and channel, strips of G-11 fiberglass composite were to be placed in the bottom of the vacuum box to transmit the load to the magnet poles.

The vacuum boxes were fabricated and initial acceptance testing revealed very poor vacuum quality with the lowest pressures achieved being about 100-mm Hg. The boxes were reworked and tested again; the small box was evacuated to 20 μ m and the large box to 100 μ m. At this time, the contract was renegotiated with the vendor and the total value of the initial contract was decreased by an amount estimated to effect the required repairs to achieve the necessary vacuum (1 to 10 μ m). The boxes were delivered to AEDC.

The large vacuum box is shown in Figs. 23 and 24. In Fig. 24, the undulations in the surface are quite evident. The channel tracks were specified to have an alignment (straightness and parallelism) of less than 0.083 mm/m. The dimensional variations in the box were obviously several orders of magnitude greater than this and it was not apparent how much of the waviness would disappear under loading. To answer this question, it was decided to measure the vacuum box lower surface flatness under simulated generator channel loading. For these measurements, tracks were installed in the vacuum box which, in turn, was mounted on a large machined table. A 3-ton cart, simulating the channel loading, was traversed along the rails and vertical displacements at three reference points on the cart were measured optically with a transit. Some flattening of the box resulting from "ironing" out the sheetmetal undulations was observed. However, significant waviness remained with the load simulation and the maximum deviation from the reference plane was found to be 1.73 cm. This waviness is unacceptable since it cannot be compensated for to achieve the required track alignment to prevent damage to the actual channel. A section of the bottom of the vacuum box was cut out, revealing that the track G-10 support pads had moved or had been inaccurately installed. Further investigation led to the conclusion that the track support system in the vacuum box was inadequate and a complete rework would be required. The small vacuum box was retested and it was found incapable of maintaining 100- μ m vacuum. Apparently, the sheetmetal welds are brittle and had deteriorated during transport or handling since its prior testing. It is assumed that embrittlement and subsequent deterioration of the welds in the larger vacuum box were equivalent to those of the smaller box. These shortcomings, coupled with the requirement that additional bracing of the large vacuum box was required due to a design oversight which neglected the ferromagnetic properties of Invar, cast considerable

Figure 23. External View of Large Vacuum Insulated Box for Lining Magnet Bore

Figure 24. Internal View of Large Vacuum Box Liner

doubt as to whether the vacuum box could ever be expected to achieve its design performance in practice.

As a consequence of the uncertainty in the vacuum box design, the problem of magnet bore insulation has been reconsidered with styrofoam as an alternative. The predicted heat loss through the vacuum box is given as a function of the vacuum pressure in Fig. 25. Clearly, partial pressures greater than 100 μm in the structure are insignificant in reducing the conductive heat transfer and full advantage of the vacuum can only be achieved at pressures of approximately 1 μm and lower. Bands of expected performance for various thicknesses of styrofoam are also indicated in Fig. 25 for comparison. A direct replacement of the vacuum box with an equivalent 2.5-cm thickness of styrofoam would equal the performance of the vacuum box demonstrated to date. Approximately 10 cm of styrofoam would provide the ultimate in performance, but required channel clearances will only allow installation of 5 cm thickness on the average and 7.5 cm at the most. It would appear that the 5- to 7.5-cm thickness of styrofoam will still equal or surpass the expected performance of the vacuum box after any reasonable reworking and would have the advantage of eliminating the vacuum pumping system. A design based on a styrofoam bore liner is being pursued and it appears that an area-weighted average insulation thickness of 6.75 cm will be achieved.

When the magnet is cooled, it can be expected that the warm bore surface temperature, even for the best insulation achievable (Fig. 25), will be between ambient temperature and 260°K. Typical summer and winter dew points are 294°K and 278°K, respectively; hence, considerable condensation will occur. The outside of the magnet is of little consequence in this regard since a vapor barrier has been provided for keeping the moisture out of the FCS enclosure. However, additional precaution will have to be taken to ensure that the bore, as well as the channel, remains dry. It is expected that sealing boots will have to be installed at the bore openings with a dry nitrogen purge in the bore itself. The original vacuum box design was not free from the potential for condensation and no protection for the channel was provided.

3.5 INSTRUMENTATION

The magnet FCS is instrumented with thermocouples and strain gages. The thermocouples are used to monitor the cooldown of the magnet to control the differential contraction of the magnet coils and the structure. The purpose of the strain gages is to monitor the strain levels in the FCS during magnet cooldown and to monitor the strain levels in the FCS during pulsed operation of the magnet. These strain data will be used to refine the load path definition in the FCS model.

The operating conditions for the strain gages are: temperature range from 77°K to 350°K, magnetic fields up to 6 Tesla, and strain levels up to the yield point of aluminum (i.e., approximately 0.75 percent strain).

Figure 25. Comparison of Insulating Properties of Styrofoam and the HPDE Vacuum Box System

Karma[®]-type gage material was chosen because it has negligible magnetoresistive effects over the desired temperature range in magnetic fields up to 6 T and it was usable over the expected strain levels.

To achieve temperature compensation, the strain gages are operated in a voltage-excited, full-bridge configuration with one active gage bonded to the structure and three "dummy" gages bonded to an aluminum tab nearby. The aluminum tab is in thermal contact with the structure but does not experience the strain. In addition, a self-temperature compensation value was chosen which is optimized for the temperature range encountered.

A room temperature setting strain gage adhesive, Micro-Measurements AE 10, was used to bond the strain gages to the aluminum structure. This adhesive has one-half to one percent elongation capabilities. Known higher performance adhesives require 325°K or greater setting temperatures. These higher setting temperatures are not compatible with the large aluminum pieces of the FCS and the requirement to install many of the gages during the actual FCS assembly process.

Further discussion of the strain gage selection and the evaluation on the tensile specimens was presented in the previous annual progress report, Ref. 3.

The strain gages were primarily located on representative highly stressed portions of the FCS. Attempts were made to locate the gages away from areas where the stress gradients were large, thus reducing the uncertainties due to the gage size and position. Most of the gages are located on the left outlet end of the structure (Fig. 26). Wherever possible, gages were mounted at corresponding locations on both sides of a structural piece; however, this was severely limited by the physical clearance available for the strain gages and the ability to get the wires out of the structure. A tabulation of the installed gages is given in Table 7. Of the gages installed, 121 were mounted in the full bridge configuration for monitoring on the data systems. An additional twenty-one gages were installed as a single gage with four leadwires. These single gages do not have the dummy compensating gages, hence require the use of the temperature obtained from a nearby thermocouple and correction curves to remove the temperature-related effects.

Thermocouples were mounted on the magnet FCS to monitor the temperature gradients encountered during cooling of the magnet. They are not intended to be used during periods when the magnet is pulsed. To meet the goal, the temperature range to be monitored is 77°K to 350°K, with a resolution approaching 3°K.

The chosen thermocouple material was ANSI Type T (copper-constantan) special grade. This thermocouple was chosen because: (1) the ANSI performance standards are specified over the desired temperature range,

Figure 26. Location of Strain Gage Instrumentation on HPDE Magnet Force Containment Structure

Table 7
Tabulation of FCS Strain Gages

Member	Location	Number Full Bridge	Number Single Gage
Left LTM	Outside	14	2
	Inside	2	2
Right LTM	Outside	15	2
	Inside	2	2
Outlet Faceplate	Outside	29	0
	Inside	4	0
Upper Outlet Tongue	Outside	6	7
	Inside	6	0
	Edge and Lip	5	0
Upper TTM No. 10	Outside	9	6
	Inside	15	0
	Edge	1	0
Left Outlet Side Saddle Plate	Outside	4	0
	Inside	0	0
Outlet Saddle Cap	Outside	5	0
	Inside	0	0
Upper TTM No. 1	Outside	1	0
	Inside	0	0
	Edge	1	0
Upper Inlet Tongue	Edge	<u>2</u>	<u>0</u>
		121	21

and (2) the homogeneity of the Type T component wires is better than for Types E or K, thereby reducing the errors due to the inhomogeneity in temperature gradients.

Wherever feasible, thermocouples from the same lot were used on one structural element. This reduces the effect of thermocouple lot on temperature gradient determinations across the element. In addition, the thermocouple wire leading out of the magnet structure is double-Teflon^(R) jacketed, 24-gage wire. The Teflon insulation was chosen to meet the temperature operating range. The wire gage was chosen for minimum wire size and availability in long lengths. In locations requiring extra durability during magnet assembly, 20-gage wire was used.

The critical thermocouple locations were chosen to monitor the temperature gradients across the LTM's (Fig. 27). These temperatures will be used to determine the transverse bending of the LTM's due to transverse temperature gradients, and to match the LTM contraction to that of the magnet coils during cooldown. Other thermocouples were spread over the structure to monitor the general cooldown progress (Fig. 28).

The strain gages and the thermocouples will be monitored by two 128-channel data loggers, one devoted to strain gages and the other devoted to thermocouples. The data loggers, sampling at the rate of 15 to 20 channels per second, will output strain and temperature data on a simple paper tape or, upon command, will be able to transfer the data to the Digital Equipment PDP 11/50 for further processing, video display, and recording on magnetic tape.

The basic strain gage data system is installed, but the strain gage hookup is incomplete and the interface to the PDP 11/50 has not been received. The complete thermocouple data logger is expected in January 1979. Hall probes will be used to measure the magnetic field strength. These are on hand and the power supplies and signal conditioning required are being fabricated.

3.6 MAGNET COOLDOWN ANALYSIS

3.6.1 Introduction

Preliminary studies of the magnet coils and FCS indicated that large thermal gradients and unacceptable induced stresses could develop within the structure during magnet cooldown. Investigation of these effects was undertaken at AEDC during the past year. Results from these analyses provide a comprehensive understanding of the transient thermal behavior of the structure and identify critical areas which require careful control during cooldown to cryogenic temperature. From

Figure 27. Thermocouple Locations on Longitudinal Tension Member (LTM) and Side Beam Module (SBM)

Figure 28. Locations of Thermocouple Installations on the HPDE Magnet Force Containment Structure

these studies, preliminary guidelines for governing the cooldown operation have been established.

Results from analysis of the HPDE styrofoam insulation indicate that it is sufficient to maintain the structure in the cold condition with a steady-state heat load of no more than 25 kw.

Investigation of the transient system behavior revealed that the critical interaction during cooldown of the structure will be the interference between the LTM's and the magnet coils. Two types of structural loading can result from this interference since the contraction rates of the LTM and the coils are unequal and nonuniform cooling of the LTM itself can occur. By controlling the cooldown operation such that the temperature difference between the coils and the structure, as well as between different sections of the structure itself never exceeds 40°K, the thermal stresses can be maintained within tolerable bounds.

Cooling of the magnet will be a lengthy process encompassing a minimum of 50 hr. It is felt that the key to avoiding unforeseen difficulties is to permit the structure to cool slowly and bring both the coil and its surrounding structure down in temperature together.

3.6.2 Cooling System Description

Cooling of the HPDE magnet will be accomplished by flowing LN₂ over free surfaces of the various structure members and through the hollow OFHC copper conductor of the coils. During cryogenic cooldown, LN₂ will be individually fed to each half-turn of the magnet coils and this flow will pass the entire length of magnet from the outlet end to the inlet end.

The manifolding arrangement used to supply coolant to the magnet coils is shown schematically in Fig. 29. Two 20-cm diam manifold headers, located on each side of the outlet end of the magnet, are used to provide flow to the entire 720 turns of the coils. Each supply header feeds coolant to nine 6.4-cm diam parallel feeder pipes, each of which, in turn, is used to supply coolant to twenty 1-cm diam coolant tubes. These cooling tubes are connected in the center of each coil turn at the saddle crossover and extend out through the FCS end box. Coolant enters the coil turns by way of the cooling tubes, is divided at the coil junction, and flows the length of the magnet in the two opposing coil half-turns. The expended coolant exits the coils at the magnet inlet end through a manifolding arrangement which is similar to the above-described magnet outlet end manifolding.

The members of the FCS which are directly cooled with LN₂ flow are the LTM, collar and faceplates, and the transverse tension members (TTM). Of these members, the first two are equipped with cooling flow channels

Figure 29. Schematic of Cooling System for HPDE Magnet Coils

over the external surfaces while the TTM will be cooled by use of LN₂ spray tubes.

Figure 30 is a schematic showing the arrangement of cooling channels on the large aluminum faceplates and collars. These channels are constructed from 0.32-cm thick aluminum sheetmetal and have a rectangular flow passage 1.27 by 12.7 cm which gives 16.13 sq cm of area for coolant flow. The faceplate cooling channels are supplied from a single supply line. Coolant enters these channels at the bottom corner of the faceplate and exits into the open structure at the opposite upper corner. The four cooling channels on the collars are individually fed with LN₂. This method of independently feeding these channels was proposed to assure a more uniform cooling of this member, since it is composed of four separate pieces and will exhibit some thermal contact resistance at the joint interfaces.

The LTM's are equipped with eight aluminum cooling channels located on the beam upper and lower edges as illustrated in Fig. 31. The four long interior channels extend a length of 2 m each, while the four shorter channels that are located within the end boxes extend 0.5 m. All channels on the LTM are of identical design as those previously described for the faceplate and collars.

Flow of LN₂ will be regulated independently to the four inlet end channels and the four outlet end channels. This type of regulation allows each end of the LTM to be cooled separately. Coolant is fed into the center of these channels, which provides a relatively uniform cooling distribution over the exposed edges.

The eleven TTM's will be cooled by an impingement of LN₂ spray from four independently supplied spray tubes. These tubes extend across the assembled members, as depicted in Fig. 32, and are fed LN₂ from the inlet end of the magnet. The spray tubes are constructed from 1.27-cm OD aluminum tubing and have spray ports which vary from 0.1 cm to 0.15 cm in diameter. The parts are sized to provide an evenly distributed LN₂ spray along the length of the tubes, accounting for the flow pressure drop which occurs along the length.

A schematic diagram of the complete HPDE cooling system piping is given in Figs. 33 and 34 for the outlet and inlet manifolding, respectively. In all cooled members, except the magnet coils themselves, expended coolant is allowed to flow freely throughout the structure once it has traversed the cooling passages. The thermal enclosure which surrounds the entire FCS is designed to contain the expended vapor and vent it to the atmosphere. This venting process will maintain a net positive pressure of approximately 2.5 cm of water within the enclosure and eliminate any cryogenic pumping of atmospheric moisture as the system is brought down in temperature. Coolant flow through the magnet coils will be recirculated to the supply system. A holding tank situated downstream of the magnet will be utilized to separate the liquid and vapor phases prior to recirculation.

FACEPLATE AND COLLAR COOLING FLOW SCHEMATIC

Figure 30. Schematic of Cooling System for HPDE Magnet Faceplate and Collar

LONGITUDINAL TENSION MEMBER
LN₂ COOLING FLOW SCHEMATIC

Figure 31. Schematic of Cooling System for HPDE Magnet Longitudinal Tension Member

Figure 32. Illustration of Cooling System for the Transverse Tension Members of the HPDE Magnet Force Containment Structure

Figure 33. Flow Schematic for HPDE Magnet Outlet End Manifold

Figure 34. Flow Schematic for HPDE Magnet Inlet End Manifold

Monitoring of the flow to all members will be accomplished by use of flowmeters located in the individual supply lines. The locations and nominal and maximum flow rates projected for cooldown operation are depicted in the manifolding schematics.

3.6.3 Thermal Steady-State Performance

Thermal insulation for the MHD magnet and FCS is provided by styrofoam sheets which cover the exposed surfaces of the structure. Illustrated in Fig. 35 is a longitudinal cross-sectional view of the structure showing the positioning of this thermal insulation. All styrofoam used is of the high density type having very good thermal insulation characteristics over the temperature range of interest. The styrofoam insulation varies in thickness from 7.5 to 12.5 cm over the entire 195 m² of outer surface area, which gives an average area weighted insulation thickness of 9.4 cm.

The MHD channel will be insulated from the cold magnet bore by a styrofoam liner presently being designed as discussed in Section 3.4. The clearance between the magnet bore and outside channel walls that is available for styrofoam insulation is depicted in Fig. 36. An area-weighted average insulation thickness for the proposed liner design of approximately 6.75 cm can be computed and the total inner wall surface area insulated by the liner is approximately 43.9 m².

Heat gain to the magnet system at cooldown temperature will be by heat conduction through the thermal insulation from the atmosphere by the natural convective process. To evaluate the overall heat load on the structure, the worst case, characterized by the maximum possible temperature gradient through the insulation, was considered. After cooldown to LN₂ temperature (77°K), a maximum temperature differential of 216°K exists relative to typical room conditions (293°K). Based on the average insulation thicknesses previously stated, thermal gradients of 23°K/cm across the outer surface insulation and 32°K/cm across the channel insulating liner are used under worst case conditions. The thermal conductivity of styrofoam insulation can vary as much as 100 percent over this temperature range; but, for analysis purposes, a mean value of the conductivity of 0.029 w/m-°K is taken for the entire magnet insulation. For these conditions, the steady-state heat transfer is calculated to be 13.01 kw across the outer surface insulation and 4.07 kw across the styrofoam channel liner. The indicated heat transfer across the styrofoam liner is, in fact, less than anticipated for the original vacuum liner. Therefore, the total heat gain to the structure with the magnet cooled to steady-state liquid nitrogen condition is estimated to be about 17.1 kw. This heat load estimate represents a first-order approximation based on mean thermal properties and insulation thicknesses and a conservative heat load estimate of 25 kw is used for design purposes.

Figure 35. HPDE Thermal Insulation

CHANNEL TO MAGNET BORE CLEARANCE

Figure 36. Clearance Available for Styrofoam Insulation of Magnet Bore

To counteract this steady-state heat gain at cooldown, a continuous supply of LN_2 will be fed into the various cooling channels of the structure and into the magnet coils. The above heat load converts into an equivalent maximum flow of LN_2 of approximately 570 ℓ/hr . Care will be taken at steady-state conditions to match the actual heat gain to the system with an equivalent LN_2 flow in order to prevent any accumulation of LN_2 within the structure.

3.6.4 Cooldown Transient Analysis

Analytical heat transfer models of the magnet and FCS were developed to predict the behavior of the magnet system during the large thermal transient which will be encountered during cooldown to cryogenic temperature. The various models employed give a comprehensive description of the qualitative behavior of the system and provide a reasonable foundation for sound engineering judgement regarding control and operation of the actual cooling down process.

The development of mathematical models to predict the behavior of the HPDE magnet structure during cooldown involves the consideration of phenomena such as: boiling heat transfer, two-phase flow, thermal contact resistance between components, and the variation of thermodynamic properties with temperature.

The variation of important thermodynamic properties with temperature for the two primary materials in the magnet system is given in Fig. 37. As indicated in the figure, the thermal diffusivity, which is a measure of how freely heat moves through a medium, varies inversely with temperature and can increase fourfold during cooldown to cryogenic temperatures for both aluminum and copper. In contrast, the thermal heat capacity for both materials becomes progressively less as temperature is reduced. The necessity to account for such extreme property variation in modeling the heat transfer of the structure is obvious and the various models employed in the present analysis, unless otherwise stated, account for such property variation.

3.6.4.1 Magnet Coil Cooldown

The coil will be cooled by a flow of liquid coolant through the internal cooling passage of the coil turns. With LN_2 as the coolant fluid, the cooldown process is specifically one of forced convective boiling. This cooling process is presently not well understood; however, in recent times, the importance of acquiring a better understanding of its nature has become apparent and more attention is being focused on development of quantitative methods of evaluating boiling phenomena.

To provide insight to the forced convective boiling process, consider the phenomena which occur as LN_2 is fed into and moves downstream in a typical cooling passage. Near the entrance of the tube,

VARIATION IN THERMAL PROPERTIES

- $\alpha/\alpha_{\text{ref}}$ (Thermal Diffusivity)
- k/k_{ref} (Thermal Conductivity)
- - - - - $C_p/C_{p_{\text{ref}}}$ (Specific Heat)
- $\epsilon/\epsilon_{\text{ref}}$ (Thermal Expansion Coefficient)

Figure 37. Variation of Thermodynamic Properties with Temperature for Two Materials in the Magnet System

liquid will be in direct contact with the cooling passage wall and boiling will ensue. Associated with this boiling will be large heat fluxes into the fluid which are directly related to the wall temperature. As the coolant progresses downstream, more and more vapor will be produced due to boiloff of the liquid. The coolant fluid will pass from a quality state of pure liquid at the entrance to one of pure saturated vapor at the end of the boiling region.

Within the boiling region itself many different types of two-phase flow patterns can be created. These patterns, termed flow regimes, depend upon several variables which include flow rate, fluid properties, and flow geometry. Accompanying the variable flow regimes possible are drastically varying heat fluxes. Such flux variation is primarily a consequence of the amount of liquid-wall contact and the processes by which bubbles develop and grow with the boiling flow.

As the flow moves farther downstream, eventually all liquid will be boiled off and at the end of the boiling region the coolant will be completely in the vapor phase. From this location on downstream, heat transfer will be purely gaseous convection which in itself can be adequately described and analyzed.

In the early stages of magnet cooldown, all liquid boiloff will occur at or very near the entrance of the cooling passage. Heat transfer in the coil turns during this initial period will result from gaseous convection within the passage and conduction within the bar. As the temperature at the entrance region is lowered and the rate of boiling subsequently reduced, the liquid phase will enter the tube and establish a boiling region of definable length therein.

Since the heat flux in the boiling regime will be appreciably greater than in the convective regime, an abrupt discontinuity in the temperature of the coil can be expected to occur at the interface of the boiling-convection regions. As cooldown proceeds and the coil temperature continues to drop, the boiling region will progressively grow longer and longer until, in the final stages of cooldown, it will encompass the entire length of the coil.

The physical description of the coolant flow and thermal gradient development as discussed above is quite well depicted by the numerical solutions of the analytical heat transfer model used to study coil cooldown. Some typical results of this model are given in Fig. 38. These results give the time-history development of thermal gradients within a coil half-turn for flow rates of 2, 4, 8, and 10 l/hr. All cases reported here were computed for constant input coolant flow rates of LN₂. The basic model used neglects the pressure drop through the cooling passage which can have appreciable effect on the boiling process and flow development for the higher flow rates. The fundamental model used to predict boiling heat flux was that of pool boiling of LN₂ at atmospheric pressure.

MAGNET COOLDOWN ANALYSIS

Figure 38. Magnet Cooldown Analysis

As illustrated in the results, during the early stages of cooldown, most of the temperature drop within the copper conductor bar will be localized to the inlet third of its total length. As cooldown time proceeds, the boiling region front enters the passage and its motion downstream can be seen in the results by following the intercept of the thermal gradient with the base cooldown line. In each case, the numerical solution was allowed to proceed until complete cooldown of the entire coil half-turn to LN_2 temperature was achieved. The cooldown times are 8.50, 4.5, 3.0, 2.50 hr for flow rates of 2, 4, 8, and 10 ℓ/hr per half-turn, respectively. These cooldown times are summarized in Fig. 39.

Once the temperature at the exit of the cooling passage begins to drop, the cooling process will become less efficient since the exhausting coolant and the conductor will not be in thermal equilibrium at the exit. Therefore, greater efficiency in terms of LN_2 consumption could be achieved by reducing the coolant flow rate at this point in time.

The motion of the boiling front is of particular interest since it is in this region that the maximum temperature gradients occur. This motion is illustrated in Fig. 40 which gives the position of the boiling front within the cooling passage as a function of time and coolant flow rate. After an initial fluctuation caused by transition cooling at the passage entrance, the boiling front can be seen to move smoothly downstream in the cooling passage.

As can be seen in the analytical results previously presented, the coil thermal gradients developed during cooldown are more severe for the higher flow rates. The larger thermal stresses which accompany these gradients demonstrate the desirability of using low operational coolant flow rates. In order to damp out any large coil thermal stresses, it is possible to shut off coolant and allow pure heat conduction to relax internal thermal gradients. The characteristic response of the coils to such a relaxation technique is typified in Fig. 41. This figure illustrates the time decay of a typical cooldown temperature gradient subject to complete adiabatic surroundings and as such, gives the maximum thermal relaxation time. Although a complete thermal relaxation of the coil could take considerable time, it is apparent from the results presented here that the steep temperature gradients can be reduced to acceptable levels in a fraction of this time, probably less than 2 hr will be sufficient.

In practice, it will be necessary to regulate the coolant flow to the coils during cooldown to assure both a matching of the magnet thermal contraction rate with that of surrounding aluminum structure and to maintain a uniform flow distribution to each of the individual coil turns. The latter requirement may prove to be particularly important during the early stages of cooldown when large pressure fluctuations associated with violent boiling of coolant at the tube entrances and in the manifold lines are anticipated. Concern has

Figure 39. HPDE Magnet Coil Cooldown Time Characteristics

Figure 40. LN_2 Boiling Front Motion in Magnet Cooling Passage During Cooldown

Figure 41. Thermal Relaxation Characteristics for HPDE Magnet Coils

been expressed that these fluctuations may affect the flow distribution and, in turn, cause nonuniform cooling between the individual turns. Since no practical means of monitoring the flow rate to each of the 1,440 flow passages is available, an intermittent regulation of the flow control valves is planned to introduce a flow pulse. Such a pulse should alleviate any temporary blockage of a turn that may develop as a result of localized back pressure buildup.

Other items which will affect the liquid coolant distribution are boiloff in the manifolding feeder lines and the effect of gravity. An unmatched insulating effect on the manifolding lines can allow for more LN_2 boiloff in some feeder tubes than others, thus making less liquid available for cooling in their respective coil turns. Gravity will tend to allow liquid to be more accessible to the lower coils. Both effects can cause nonuniform cooling within the magnet and may be especially prevalent during the early phases of cooldown.

From a review of the analytical results on the magnet, it is concluded that a complete uniform cooling of the coils should be attainable provided that a uniform distribution of coolant to the individual turns can be maintained.

In view of the operational problems anticipated with obtaining such a distribution, it appears that an intermittent regulation of the coolant flow rate will provide the most promising means of assuring uniform cooldown. Such regulation can consist of either increasing or decreasing the flow to all or part of the coils. In addition, the flow can be shut off entirely, if deemed necessary to allow thermal relaxation of the coils.

At present, experimental apparatus is being constructed to physically cool a single 10-m section of hollow copper magnet conductor material to LN_2 temperature. Information will be gained from this test regarding control and operation of cooldown as well.

3.6.4.2 Force Containment Structure Cooldown

Cooling of the FCS will proceed simultaneously with coil cooldown. The rate at which this massive structure can be brought down to cryogenic temperature is dependent upon the available surface area exposed to LN_2 and the freedom with which heat can move throughout the structure by conduction. It is this cooldown rate which shall determine the total time required for complete magnet cooldown. Of the FCS members directly cooled by LN_2 flow, the LTM is considered to be the most critical. It is through this beam that all longitudinal loads which develop in the structure will be transmitted.

In studying the LTM cooldown, a pseudo-three-dimensional heat transfer model was used in which it was assumed that boiling of LN_2

would occur within the upper edge cooling channels and that gaseous nitrogen convection would occur in the lower ones. These assumptions are physically plausible when the effect of gravity on the LN_2 coolant is considered. Gravity will enhance liquid-surface contact on the upper beam edge giving rise to LN_2 boiling and high heat fluxes there. In the lower channels the liquid phase accumulates along the channel roof and thus allows LN_2 vapor to be in contact with the lower LTM edge. To simplify the model, the boiling process was taken to be nucleate pool boiling rather than the forced convective boiling process that was used in the coil cooldown analysis. In addition to the cooling on the LTM edges, there will be significant heat gain or loss to this member due to conduction through its inner and outer faces.

For the purposes of analysis, the coil side of the LTM is taken to be insulated. This assumption is based on the premise that the coils will be brought down in temperature at the same time as the LTM, thus reducing the thermal potential between these members. Also the coils and LTM are physically separated by a sheet of G-10 fiberglass which will also serve to reduce heat transfer there. Heat gain from the side beam modules to the LTM is accounted for in this study and gives the model a three-dimensional effect.

The results, as given in the first three graphs of Fig. 42, are typical of the type of response anticipated in LTM during cooldown. These three represent a nominal lower surface convective condition. The two-dimensional growth of isotherms, which is a consequence of the asymmetric heat removal at the edges, is quite well depicted in the results shown. Total cooldown of the LTM using this model is predicted to be a lengthy process encompassing more than 50 hr total time.

Also illustrated in Fig. 42 are isotherms developed in the LTM for an adiabatic lower edge. This case is considered the most severe situation which could be encountered in cooldown of the LTM and, in fact, simulates a loss of coolant flow to the LTM's lower cooling channels. Comparison of the adiabatic case to the convective lower surface case at an identical time can be made in this figure. As expected, the adiabatic case shows both a slower cooling rate and steeper thermal gradients.

Superimposed on Fig. 42, along side of their respective isotherms, are thermally induced stresses. These are stresses which would have the overall effect of causing a bending or warping of an unrestrained LTM. As illustrated, the adiabatic lower surface case results in the greater nonsymmetric thermal stress distribution and therefore typifies a worst case warping of the beam itself. This worst case was utilized in developing the worst thermal loading case expected in the magnet structure.

Severe thermal gradients and their corresponding bending stresses which can develop in the LTM may be relieved by a periodic thermal

LONGITUDINAL TENSION MEMBER
TYPICAL COOLDOWN CHARACTERISTICS
BOILING ON UPPER SURFACE
ISOTHERMS AT DIFFERENT TIMES AFTER INITIATION OF COOLDOWN

NOTE: ALL TEMPERATURES IN °K

CONVECTION ON LOWER SURFACE $\sim h = 25 \text{ BTU/HR-FT}^2\text{-}^\circ\text{R}$ ($142 \text{ WATTS/M}^2\text{-}^\circ\text{K}$)
10 HR

CONVECTION ON LOWER SURFACE $\sim h = 25 \text{ BTU/HR-FT}^2\text{-}^\circ\text{R}$ ($142 \text{ WATTS/M}^2\text{-}^\circ\text{K}$)
20 HR

CONVECTION ON LOWER SURFACE $\sim h = 25 \text{ BTU/HR-FT}^2\text{-}^\circ\text{R}$ ($142 \text{ WATTS/M}^2\text{-}^\circ\text{K}$)
35 HR

ADIABATIC LOWER SURFACE
35 HR

Figure 42. Thermal-Time History of Longitudinal Tension Member During Cooldown

relaxation of this member. As in the case of the coils, this can be accomplished by regulation of coolant flow rate to the various channels of the beam.

The other members of the magnet FCS which were studied from a heat transfer point of view were the transverse tension members (TTM). The model used to simulate the cooling of these members was one-dimensional cooling of a thin plate by conduction from its ends. The end cooling exemplifies the impingement of LN_2 on the TTM at the two ends from the spray bars as previously described. For the analysis of this member, constant thermodynamic properties were assumed and two convective heating rates typical of boundary values were considered. The results of this model are depicted in Fig. 43. The development of thermal gradients in the TTM is much less severe for the lower convective heat rate than for the high one. As can be inferred from these results, large gradients can be avoided in the TTM by allowing heat conduction within the member to be the dominant mode of heat transfer. It is presently planned to cool this member slowly and bring it down to cryogenic temperature as uniformly as possible in this fashion.

3.6.4.3 Thermal Loading

Large thermally induced forces can occur within the magnet structure due to unmatched thermal contraction rates of the various structural components. The cooldown process must be controlled to ensure that these forces are kept within acceptable limits. The structure has several built-in gaps between adjacent members and some slack will be present in the final assembly, which will serve to absorb a portion of the overall thermal contraction. Drawing on the results of the transient thermal analyses previously presented, the pertinent physical loads which can develop during cooldown and their interaction with other members of the structure are considered in the following discussion. A transverse cross-sectional view of the assembled magnet structure taken at its center is presented in Fig. 44. This drawing illustrates the relative positions of internal structural components and serves as an aid in describing thermal loading which can develop in this plane. As indicated, the TTM ties the structure together in the transverse plane. Thermal contraction of these members during cooldown will have the undesirable effect of pulling the internal members together and loading the structure. Conversely, thermal shrinkage of the internal members: the coils, the LTM, and the side beam modules, will cause these to pull away from one another and this shrinkage will tend to relieve any loading caused by the TTM contraction. Therefore, no adverse thermal loading will develop in the transverse plane provided that the TTM's are cooled at a slower rate than the rest of the structure. This method of cooling is presently planned for the actual magnet cooldown.

Figure 43. Thermal Response of an End Cooled Plate as used to Model Cooldown of Transverse Tension Members

Figure 44. Transverse Cross Section of the Force Containment Structure

In the longitudinal plane of the structure the thermal induced loading is more complicated. All motion of the FCS during cooldown will be with respect to its inlet end since this end is securely fastened to the foundation. The outlet end of the structure is free to move with 3 deg of freedom, its only restraining forces being the structure weight and friction. To illustrate such motion, Fig. 45 shows a cutaway drawing of the outlet end box. This figure depicts the relative position of selected structural components prior to and after total cooldown. Referring to this illustration, it can be seen that before any cooling, the outlet end box will be situated away from the TTM's and the steel yoke. As cooldown proceeds, the thermal contraction of the LTM will longitudinally displace the end box closing the available gap at the outside surface of the collar. Eventually, the collar will contact the first TTM and physically move it; a 1.6-cm relief is provided for TTM movement. Further contraction of the LTM will bring the outside collar face in contact with the styrofoam insulation which covers the steel yoke blocks. Eventually, crushing of this styrofoam covering will occur, but will be limited by embedded blocks. At final cooldown temperature, the total movement of the outside collar face relative to the inlet end of the structure will be 2.78 cm.

Inside the end box, interference between the coil crossovers and surrounding structure can occur. If the coils are allowed to cool at a rate faster than the LTM, this interference will be with the inside surface of the collar; whereas, if the coils are cooled slower, it will be with the inside surface of the faceplate. Both types of interference can produce a net force on the coils of significant magnitude if these cooling rates are appreciably different.

At the final cold state, the total system contraction will show the coils to be in contact with the faceplate. At that contact surface, the estimated thermal shrinkage of the coils is 2.69 cm while that of the LTM is 3.48 cm. With the 0.25 cm gap available at this location prior to cooldown, a 0.54 cm interference between these members will be present at cooldown which corresponds to an estimated coil preload of 1,335 psi.

The above description of longitudinal motion during thermal cooldown was taken from the estimated gaps in the structure at the time this analysis was undertaken. The final assembly will undoubtedly be somewhat different and these proposed gaps in the structure need to be re-evaluated in terms of their effect on thermal contraction behavior.

In addition to this axial loading which results from unmatched overall thermal contraction, another type of loading on the coils is predictable. This second type is a direct consequence of nonuniform cooling of the upper and lower edges of the LTM. As was previously discussed in the thermal analysis of this beam, uneven cooling of the LTM upper and lower edges will introduce the tendency for this beam

THERMAL CONTRACTION INTERACTION

Figure 45. Illustration of Coil Force Containment Structure Motion and Interferences at Cooldown

to warp about its longitudinal axis. To counteract such warpage, a force will be generated in the assembled structure by interaction of the magnet coil crossovers and the end box. This force will be transmitted to the LTM in the form of a bending moment. The overall effect of the bending load is to produce an additional load on the magnet coils which must be superimposed onto the axial mean thermal contraction loading.

These two types of loads are illustrated in sketch of Fig. 46. Their combined effect is considered the most crucial loading which can occur during cooldown. Guidelines set forth to govern the cooldown operation are designed to assure proper control of this LTM coil interaction and maintain the resulting physical loads within conservative limits.

In analyzing the stress which develops in the magnet coils due to this type of loading described above, the coil was taken to be a curved beam of solid cross section. In reality, the pancake type construction of the coil will allow slippage between the individual pancake layers of the copper coils and this slippage will serve to relieve some of the stressing. Also, the pancake construction will allow a certain amount of compression at the outer saddle crossover of the coil before any significant stressing can begin to develop. Therefore, the rigid curved beam approximation will yield higher stress values than can be expected to occur in the actual case. A curved beam analysis (Winkler-Bach method) was employed to predict the coil stresses. This method gives the maximum stress at the coil bending radius and is summarized in Fig. 47a.

The loads applied in the stress evaluation are computed as the combined axial and bending thermal loads. A total contraction relief of 0.64 cm was used in the analysis to realistically account for the built-in gap and slack of the assembled structure. Therefore, a contraction interference between the outlet faceplate and coil of this amount or less was considered to be the lower limit for the load-free operation. The thermal loading caused by LTM warpage was derived using the results of the worst case, adiabatic lower edge condition, heat transfer transient analysis of this member. This case produces the largest thermal gradients across the height of the LTM and correspondingly the most severe stress distribution and resultant bending moment. From the worst case heat transfer analysis, a maximum temperature differential of 40°K across the LTM was taken and a typical stress distribution for this temperature gradient is sketched in Fig. 47b. The resulting bending moment necessary to completely restrain any beam deflection can be evaluated by integration of this stress distribution. To obtain conservative estimates and simplify evaluation of the bending moment, the thermal gradient across the LTM and resulting stress distributions as depicted in Fig. 47b were linearized. Integration of the linearized stress distribution was carried out for a range of temperature gradients and a map of resulting magnet loads was calculated.

THERMAL COOLDOWN INDUCED LOADING
INTERACTION BETWEEN LTM AND MAGNET

AXIAL LOAD INDUCED DUE TO
UNEQUAL CONTRACTION OF
ALUMINUM LTM AND COPPER MAGNET.

BENDING MOMENT INDUCED DUE
TO UNEVEN COOLDOWN OF THE LTM

Figure 46. Diagram of Thermally Induced Loading Interactions between the HPDE Magnet Coil and Force Containment Structure

MAGNET STRESS EVALUATION

Winkler-Bach Equation for Maximum Stress in a Curved Beam

$$\sigma_A = \frac{P}{A} + \frac{P R}{r A} \left[1 - \frac{1}{Z} \frac{c}{r-c} \right]$$

P - Resultant thermal load

R - Moment arm

A - Cross-sectional area

r - Radius of curvature

c - Centroidal axis of beam

a - Point of maximum compressive stress

$$Z = \frac{1}{3} \left(\frac{c}{r} \right)^2 + \frac{1}{5} \left(\frac{c}{r} \right)^4 + \dots$$

a. Maximum Coil Stress Prediction

b. LTM Thermal Stress Distributions

Figure 47. HPDE Coil Stress Analysis

A summary of the thermally induced magnet coil stress predictions by the method discussed above is plotted in Fig. 48. This plot gives the predicted maximum coil stress as a function of both the LTM and coil mean temperature difference and the temperature gradient across the LTM height. The zero ΔT_{LTM} temperature line shown here is representative of stress development due to the axial thermal contraction effect alone. The various other ΔT_{LTM} lines indicate the combined effects of this axial loading and the LTM thermal warping.

Maintaining the coil stresses as predicted here to an acceptably safe level can be accomplished by control of these two temperature differentials.

The maximum coil stress evaluation was used to generate a safe magnet cooldown operating range. This range is depicted as the shaded region shown in Fig. 49. By controlling the difference between the average coil temperature and the average LTM temperature and controlling the maximum thermal gradient across the LTM to yield a combined point within the shaded area, maximum stresses in the coils should not exceed 10,000 psi. This type of approach is tentatively planned as a primary cooldown guideline. As an intended conservative bound on these temperature differentials, 40°K will be considered as the maximum allowable differential providing an additional margin of safety.

The control of cooldown will be achieved by continuous monitoring of the thermocouple instrumentation during cooldown. If it becomes apparent during this monitoring that safe temperature limits are about to be exceeded, the flow of LN₂ coolant will be regulated accordingly to bring the temperatures back to acceptable levels. This ability to either increase or decrease LN₂ flow will be the primary means of controlling thermal gradients. Under extreme conditions the coolant flow can be completely shut off to any one member of the structure to allow total or partial relaxation of its thermal gradients. In addition to the flow control, the ability to actually heat the magnet coils by introducing an electric current through them also can be used as a means of temperature control.

The average coil temperature will be inferred during cooldown by measuring its electrical resistance. The electrical resistance is functionally dependent on the coil's mean temperature as shown in Fig. 50. Since direct instrumentation of the coils with thermocouples was impossible without compromising their electrical insulation, no direct measurement of coil thermal gradients can be obtained. However, an inference of such gradients can be made by observing instrumentation on structural members adjacent to the coils; in particular, those thermocouples fastened to the aluminum separator plate between the upper and lower coil sections.

COIL STRESSES VS OPERATING TEMPERATURES

Figure 48. Summary of Coil Stresses for Thermal Gradients in the Longitudinal Tension Member and between this Member and the Coil

HPDE COOLDOWN OPERATING LIMITS

Figure 49. Envelope of Safe Operating Region during Cooldown of the HPDE Magnet to Limit Coil Stresses

Figure 50. Pancake and Total Coil Resistance vs Temperature

3.6.5 Magnet Cooldown Plan

Based on the results of the cooldown analyses performed thus far, a tentative plan of action has been set forth for the actual magnet cooldown process. To begin cooldown, the entire structure and the coils will be purged with dry nitrogen gas to drive out any residual moisture that may be present. Humidity measurements will be taken to indicate when the internal atmosphere is dry enough to proceed with LN₂ cooling. Once this requirement has been met a nominal flow of liquid nitrogen will be fed into the outlet end channels of the LTM and this condition will be maintained until a mean temperature drop in the LTM of 20°K is reached. During this initial phase of LTM cooling, the structure will pull together in the longitudinal plane and observations of key strain gages within the structure can be made. These observations will give indication of the assembled structure slack and provide insight on response of the structure to liquid nitrogen flow.

The next step in the cooldown procedure will be to introduce cooling flow to both the magnet coils and the cooling channels of the outlet end box. During this phase of operation, the entire outlet end of the structure is undergoing a thermal transient. Monitoring of all thermocouple instrumentation will be required to ensure that a maximum 40°K temperature differential between members is not exceeded. Observations during this phase will be made of the relative heat diffusion abilities of the structure and coils. Outlet end cooling will continue until a drop of 100°K in the coil mean temperature is reached. At that time, coolant flow will be introduced into the inlet cooling channels of the LTM and cooldown will proceed until the mean temperature of the LTM and coils in the neighborhood of 150°K is achieved.

In the final step of cooldown operation, all available cooling channels will be fed with LN₂. Complete LN₂ flow will be continued until the entire structure reaches a cryogenic steady-state condition.

3.6.6 Cooldown Analysis Summary

In reviewing the thermal cooldown analyses that have thus far been performed on the coils and FCS, it should be noted that the analytical models can at best give only a qualitative description of magnet cooldown behavior. The complexity of the structure along with the many variables which influence cooldown make any confident quantitative evaluation of the integrated structure quite impractical. Some of the more pertinent conclusions drawn from this present study can be summarized as follows:

1. Cooldown of the entire magnet structure to liquid nitrogen temperature will require more than 50 hr time and require a minimum of 72,000 ℓ of LN₂.

2. The thermal insulation provided for the system along with a minimal flow of LN_2 to the structure should be adequate to maintain the cold steady state.
3. The most critical interaction of structural members during cool-down occurs from interference between the LTM and the magnet coils. Severe coil stress can develop from both an unregulated cooldown of these members and nonuniform cooling of the LTM's themselves.
4. The thermocouple instrumentation and flow control system provided for the structure will allow control of the cooldown process.
5. As a guideline to safe operation, it is proposed that a maximum temperature differential of 40°K between any two members not be exceeded.

IV. SHAKEDOWN AND OPERATION

4.1 BURNER OPERATION

Burner operation was carried out intermittently throughout the fiscal year in an effort to develop a reliability in all systems and to obtain plasma diagnostic information. This operation was carried out with a flow diagnostics section installed between the burner and dummy channel as shown in Fig. 51. The diagnostics section contained four ports which could be used for flow visualization or mounting flow diagnostic probes. A photograph of the experimental setup is given in Fig. 52. The problems encountered in the previous fiscal year with the water-cooled copper nozzle blocks (Ref. 3) were solved with a set of heat sink blocks capable of operation for up to about 20 sec. After installation of these blocks, confidence was developed in the routine operation of all burner systems for a wide range of N_2/O_2 ratios, burner mass flows, and run durations, with and without seeding and automatic system control. The N_2/O_2 ratio ranged from 0.6 to 1.25, mass flow from 40 to 50 kg/sec, and run durations were from 3 to 26 sec. Ample evidence of plasma combustion uniformity and the completeness of combustion was obtained from a careful study of pressure, mass flow, and other diagnostic information.

A summary of all runs attempted in the current fiscal year is given in Table 8. Of the first nine runs (002-001 to 002-010), three were successful. The horizontal and vertical total pressure rakes were installed at the nozzle exit on these runs. Careful studies of the sensitivity of the pilot light to oxidizer/fuel ratios in December 1977 (listed at Runs 002-008 and 002-009) confirmed the need for a leaner pilot mixture for reliable ignition. The pitot-static probe was installed in the electrode test assembly for the next four runs (003-001 to 003-004).

Effort was next directed toward solving the automatic controller problems which were preventing successful burner operation in the fully automatic mode. These problems involved the sequential timing for the startup and shutdown of the various systems, limits set on several permissive functions in the process, and several software and control logic errors. Four runs were devoted to this controller development (003-005 to 003-008), with a successful fully automatic run on June 8, 1978. Subsequent operation through the summer of 1978 involved checkout of the seed system in the manual and automatic modes in conjunction with various diagnostic efforts, including infrared emission-absorption experiments, plasma conductivity, and plasma enthalpy measurements (Runs 003-009 to 003-015).

Operation of the burner was terminated on August 14, 1978, due to damage to the main high pressure cooling water pump motor. Small intermittent (tack) welds in a sheetmetal cooling air shroud had fatigued

a. Cross Section

b. Elevation View

Figure 51. Diagram of Flow Train used for the Checkout of the HPDE Burner

Figure 52. Photograph of HPDE Facility Configuration for Burner Checkout and Plasma Diagnostics Testing

Table 8

Burner Operation Summary

INSTRUMENTATION TYPES:

A - Total Pressure Rakes
 B - Traversing Pressure Probe
 C - Electrode Test Assembly (ETA)
 D - Heat Sink Thermocouple Plug Assembly
 E - Camera and Film
 F - Viewing Port Thermocouple Assembly
 G - Infrared Absorption-Emission Apparatus
 H - Conductivity Probe
 I - Enthalpy Probe

TEST NO.	DATE	DURATION SEC	TOTAL FLOW KG/SEC	N ₂ /O ₂	INSTRUMENTATION TYPE	EXPERIMENTAL OBJECTIVES	COMMENTS
M1-002-001	11/08/77			1.52	Diagnostic sect. installed	Burner firing, auto sequence	Run terminated due to water flow press.
M1-002-002	11/09/77			1.52	Diagnostic sect. installed	Burner firing, auto sequence, no pilot purge	Igniter lit four out of five times. Pilot failed twice. Run terminated due to low oxidizer pressure.
M1-002-003	11/15/77	4	52	0.872	A	Auto sequence, no pilot purge, igni- tor stoichiometric, 0.2 sec delay on pilot oxidizer.	Good run. Pitot rake data obtained.
M1-002-004	12/09/77			0.695	A	Auto sequence	Run terminated due to controller problems
M1-002-005	12/14/77			1.26	A	Auto sequence	Run terminated due to controller problems
M1-002-006	12/16/77			1.15	A	Manual attempt	Pilot failed nine times. N ₂ /O ₂ sample checked.
M1-002-007	12/19/77			1.15	A	Manual attempt	Pilot failed. N ₂ /O ₂ sample checked.
M1-002-008	12/22/77			1.125	A	Pilot light tests, vary O/F ratio to pilot	No igniter light due to blockage in oxidizer port.
M1-002-009	01/03/78			1.125	A	Pilot light tests, vary O/F ratio to pilot	Pilot lights achieved at various rates.
M1-002-010	02/04/78	2 at 4 ea.	49 & 47	0.877	A	Manual burner firing, flow uniformity, two runs	Two good runs. Pitot rake data obtained.
M1-003-001	02/17/78	3	50	0.877	B,C,D	Manual burner firing, flow uniformity	Good run. Pitot static traverse. New water cooled nozzle blocks.
M1-003-002	04/21/78			0.877	B,C,D	Manual burner firing, flow uniformity	No pilot light due to oxidizer valve. Heat sink nozzle blocks.
M1-003-003	04/26/78	3.6	41	0.626	B,C,D,E	Obtain diagnostics	Good run. Manually fired. Pitot static pressure probe on centerline.
M1-003-004	05/01/78	6.0	40	0.626	B,C,D	Obtain diagnostics	Good run. Manually fired.
M1-003-005	05/16/78			0.626	C,D	Obtain diagnostics	Test aborted due to auto controller malfunction.
M1-003-006	05/18/78	3.2	40	0.626	C,D	Obtain diagnostics	Good run. Manually fired.
M1-003-007	06/02/78			0.626	F	Auto controller and seed system checkout	Test aborted due to auto controller malfunction.
M1-003-008	06/08/78	3.8	37	0.626	F	Auto controller checkout	Good run. Fully automatic firing achieved.
M1-003-009	06/09/78	12.5	36	0.626	F	Seed system checkout.	Good run. Manually fired.
M1-003-010	06/28/78	26.0	41	0.877	G	Obtain diagnostics, seeded	Good run. Fully automatic. I-R data obtained.
M1-003-011	06/29/78	10.9	39	0.877	G	Obtain diagnostics, seeded	Good run. Fully automatic. I-R data obtained.
M1-003-012	07/21/78	5.8	41	0.877	C,H	Obtain diagnostics, seeded	Good run. Conductivity probe traversed.
M1-003-013	07/24/78			0.877	C	Obtain electrode cap erosion and heat load	Run terminated due to low pilot fuel pressure.
M1-003-014	07/28/78	13.0	41	0.877	C	Checkout larger fuel spuds, obtain electrode cap erosion and heat load	Good run. ETA data obtained.
M1-003-015	08/07/78	13.0	40	0.877	I	Obtain diagnostics	Good run. Enthalpy probe failed.
M1-003-016	08/14/78		40	0.877	B	Obtain diagnostics	Run terminated due to loss of water pressure (Pump motor failure).

sufficiently during the 15 years operation of this motor to permit the shroud to cause damage in the motor windings. This raw water pump motor is also utilized in the operation of other test units in the High Temperature Laboratory (HTL) Building. The fatigue of the tack welds had occurred after or were undetected during the previous annual motor disassembly and inspection conducted in December 1976. Repair of the motor required about four weeks; however, it was necessary to remove the burner in order to prepare a track system required for the final placement of the magnet, which it was not felt advisable to delay.

Some further seed system and low pressure water system checkout runs were made without attempting actual combustion in the burner. The burner, dummy channel, diffuser and breech were then completely removed from the magnet track work area by August 31, 1978.

4.1.1 Operating Sequence

The operating sequence has been developed over the past year to produce reliable starts at burner operating conditions required for the HPDE. The operating sequence involves 1) establishing the cooling water flow to all burner components, 2) initiating N_2 purge into selected systems, 3) starting the fuel pump and establishing the desired fuel flow on bypass, 4) lighting the igniter, 5) lighting the pilot, 6) initiating oxidizer flow through the burner, and 7) switching the fuel from bypass into the burner to produce combustion when the oxidizer is at the required flow rate. After the combustor pressure rises sufficiently to confirm ignition, the seed is injected into the fuel line just upstream of the fuel switching valve, V5-3. For subsequent powered MHD runs, it is envisioned that the magnet will be powered up just after Item 6, such that a uniform field will exist by the time Item 7 occurs. This operating sequence has been accomplished in both a manual and a fully automatic mode. Extensive refinements to the automatic control programs have been required to produce a safe, reliable operation of the entire burner system including the many potential shut-down conditions. Optimization of this automatic program is continuing and manual operation has been used successfully while automatic controller problems were being identified and solved. However, even in manual control, the 14/30 controller provides system interlocking functions.

A series of igniter-pilot firings were conducted early in the fiscal year in an attempt to duplicate the pilot misfire experience of FY77 (Ref. 3). Successful ignition of the pilot continued to present a problem early in the year; however, several modifications were made to the system timing and the pilot oxidizer-fuel ratios were increased substantially. Increasing the pilot O/F ratio has eliminated this operational problem. Instrumentation has been progressively added to the igniter, pilot, fuel and oxidizer systems in order to diagnose and improve burner operating reliability.

Reliable burner ignition has been achieved during this fiscal year for most of the runs on which the pilot has lit. As a result, the pilot ignition, indicated by a pilot chamber pressure reading, is used as a permissive in the automatic sequencer to permit the initiation of main oxidizer flow and eventual fuel flow. Only one failure of the fuel valve, V5-3, to open when commanded has been experienced and it is expected that, once the magnet is committed to a pulse, the probability of a successful burner run is high.

A simplified schematic of the oxidizer, fuel, and seed systems is given in Fig. 53. Selected line sizes, control valve sizes, and the location of flowmeters are noted. The flow rates from the oxidizer, fuel, and seed systems are plotted as a function of time for a typical seeded run in Fig. 54. The flow rates measured by each flowmeter lead the instantaneous flow rates to the burner, however, and an attempt has been made to empirically adjust the flow rate of each system to represent a probable flow to the burner itself. These empirical adjustments are made based on considerations of line volume and line length downstream of the control valve/flowmeter. The time required to establish the oxidizer flow is especially long, about 20 sec due primarily to the rate at which the control valve (V6-1) opens and to its remote location relative to the burner. The time required to stabilize the fuel flow to the burner is less than 1 sec because the operation of the valve (V5-3) is rapid, it is located close to the burner, and the fuel flow is pre-established on bypass (control valve already open). The seed valve (V4-15) is also close to the burner, and approximately 1 to 1.5 sec are required to establish steady seed flow. The seed valve is not opened until after V5-3 is opened, to prevent contamination of the fuel system, but can be opened at any desired time after V5-3 opens. The burner stagnation pressure variation, shown in Fig. 55, indicates that the critical pressure ratio is achieved in about 0.25 sec based on the assumption of isentropic one-dimensional flow.

4.1.2 Burner Heat Loss

Extensive instrumentation is present in the HPDE burner system to measure the cooling water flow rate to, and water temperature rise in, several components. The heat loss from each component is calculated utilizing an energy balance based on the heat gained by the cooling water. A schematic of the water cooling system, showing the location of the flowmeters and the temperature rise thermocouples, is given in Fig. 56. Heat loss to the igniter, pilot, pilot throat, burner back-plate, burner liner, burner extension liner and the nozzle-diagnostics section is monitored on each run. The steady-state heat loss to each of the major burner components, nondimensionalized by the total theoretical heat in the burner, is given for all burner runs conducted to date in Fig. 57. The theoretical total heat in the burner is based on measured fuel and oxidizer flows, the enthalpy of the fuel and oxidizer, and the lower heat of combustion of the fuel. It can be seen

Figure 53. Schematic of the Oxidizer, Fuel, and Seed System for the HPDE Burner

Figure 54. Typical Flow Rate Traces from a Seeded Burner Run

Figure 55. Typical Burner Pressure Transient during Startup of the HPDE Burner

RUN 003-015 - 40 kg/sec

⊙ Measured Temperature Rise, ΔT

Measured Water Flow Rate

* Flow Rate Division Based on Pipe Area Ratio

Figure 56. Schematic of Coolant Flow System for the HPDE Burner Showing Typical Coolant Flow Rates and Temperature Rises

SYM	RUN NO. M1-001	T _{Theory}	
		°R	°K
●	003	5400	3000
■	006	5400	3000
◇	008	5700	3167
▲	009	5400	3000

SYM	RUN NO. M1-002	T _{Theory}	
		°R	°K
▲	003	5300	2944
■	010A	5400	3000
●	010B	5400	3000

SYM	RUN NO. M1-003	T _{Theory}	
		°R	°K
◇	006	5800	3222
◇	008	5800	3222
◇	009	5800	3222
◇	010	5600	3111

SYM	RUN NO. M1-003	T _{Theory}	
		°R	°K
◇	011	5500	3056
×	012	5600	3111
◇	014	5600	3111
◇	015	5600	3111

Open Sym = 40 Kilo
Solid Sym = 50 Kilo
Unflagged = ≤ 6 sec.
Flagged = > 6 sec.

Figure 57. Measured Heat Loss Rates to Various Components of the HPDE Burner

that the largest heat loss, about 2 percent, is to the burner liner. The heat loss to the backplate is about 0.25 percent and the loss to the burner extension is about 0.5 to 0.75 percent. The heat loss to the nozzle is presented for the early runs utilizing the water-cooled blocks for which data are available. The total heat loss to the burner system is given at the bottom of Fig. 57, assuming that the heat loss to the water-cooled blocks, obtained on the first two runs, is representative for all runs. The total loss ranges between 3.0 and 4.5 percent. No systematic dependency of the heat loss on the forced convection parameter, $Re^{0.8}$, is detectable within the data scatter; however, a very general dependency of heat loss on the radiation parameter, T^4 , is evident. In general, the hotter runs (lower N_2/O_2 ratio) exhibit a higher heat loss independent of the burner mass flow. For purposes of estimating the heat loss, the relationship, $(Q_{loss, total})/(H_0 \dot{m} T_0^4) \simeq 4 \times 10^{-17}$, is valid for the normal HPDE burner operating conditions. The steady-state heat flux to each component, based on an average of the data presented in Fig. 57 and the surface area of each component, is presented in Table 9.

Table 9

Average Heat Transfer Rates to the HPDE Burner
for a Total Heat Input 250,000 Btu/sec

Burner Component	Area, ft ² (m ²)	Average Heat Loss, Btu/sec (kw)	Average Heat Flux, Btu/ft ² -sec (kw/cm ²)
Backplate	3.0 (0.279)	630 (0.185)	210 (0.238)
Burner Liner	18.8 (1.747)	4,800 (1.46)	250 (0.284)
Burner Extension Liner	7.3 (0.678)	1,550 (0.45)	200 (0.227)
Nozzle	3.85 (0.358)	2,000 (0.59)	500 (0.568)

4.1.3 Fuel and Seed Systems

Extensive information on the flow characteristics in the fuel and seed systems was obtained on the latter runs during this fiscal year. It appeared that the pressure drops in the fuel supply system were such that the combined fuel and seed flows required for a 50-kg run could probably not be achieved with the existing setup. Modifications were made to the fuel system and four additional pressure readouts were added to the seed system to verify the performance improvement.

A schematic of the fuel and seed systems is given in Fig. 53. The area of concern involved the pressure drop between the fuel-seed manifold and the burner chamber, ΔP . This drop is across the sixty fuel spray

nozzles, called fuel spuds, in the burner backplate. The backplate is shown schematically in Fig. 51a. Each spud has seven orifices, and thirty spuds with orifices of 0.66-, 0.79-, and 0.94-mm each are available. A photograph of the fuel spuds is given in Fig. 58. Initial testing utilized a combination of thirty of each of the two smaller spud sizes. The pressure drop, ΔP , measured on several burner runs is given as a function of the fuel flow rate in Fig. 59. Two problems are evident: 1) The fuel system pump would only be marginally capable of supplying fuel at sufficient pressure for the eventual high performance demonstration runs at 4 T if potassium seed is utilized, and 2) since the seed tank (see Fig. 53) is only pressurized to 850 psi (5.86×10^6 N/m²) the seed flow decreases drastically as the fuel line pressure increases and extensive seed system modifications would be required to achieve the required flow.

The fuel spuds were changed prior to the last two burner runs to a mix of 30 each of the two larger sizes. The pressure drop was significantly decreased as shown in Fig. 59. In addition, four pressure readings were obtained during these runs: at the seed tank, in the seed line ahead of the control valve, downstream of the control valve, and downstream of the point of seed injection into the fuel line (see Fig. 53). These pressure data confirm that the required fuel and seed flows can be delivered to the burner with the larger spud mix even if low N₂/O₂ operation is necessary for 4 T testing.

Some problems were encountered in maintaining a homogeneous seed-methanol solution during the course of combustor testing. The seed solution is mixed and stored in a holding tank which is heated to about 320°K with the solution continuously agitated. The last three seeded runs were made at intervals of one and two months after the seed was initially mixed. A sample of seed solution checked after the last seeded run revealed the presence of a significant quantity of precipitant of heavy seed crystals with a jelly-like consistency. The heavy crystals settled to the bottom of the container immediately upon termination of the agitation. The seed tank heater probably produced a vaporization of the methanol resulting in a progressively denser seed mixture with time. Use of the heater has since been discontinued. The seed solution run tank is not agitated and it is certain that the seed mixture actually injected into the burner (from the bottom of the run tank) became significantly more crystalline during the course of the two months of running with seed. Evidence now exists that the combustor flame temperature was reduced on these last seeded runs. For future runs, the seed will be mixed in small batches prior to the run to alleviate the problems associated with long-term storage.

4.1.4 Nozzle

Operation of the HPDE burner was initiated in FY77 with a water-cooled nozzle section. The nozzle is a three-dimensional contraction

Figure 58. Fuel Spray Nozzles (Spuds)

Figure 59. Fuel Flow Rates for Different Injector Combinations as a Function of Injection Pressure

from the 61-cm diam burner to the 25- by 48-cm rectangular MHD channel entrance. The nozzle was fabricated from 2.5-cm copper plate, gun-drilled for numerous water passages. After drilling, the plates were formed into the desired contour. Several problems occurred during the fabrication of this contraction section, including fidelity of water passage drilling, copper welding, and water passage patching/repair. These problems were enumerated in Ref. 3 and the nozzles failed during the third burner run on May 16, 1977. Two subsequent short runs were made with these blocks in a heat-sinking mode in spite of significant cooling water leaks.

Operation of the burner was initiated in the current fiscal year, utilizing these blocks in spite of the leaks. Three runs were made before two new water-cooled blocks were installed in February to replace the damaged, leaky blocks. One burner run was made and, subsequent to this run, one of the larger nozzle blocks was found to be displaced from the mounting flange. Closer inspection revealed a similar distortion on the mounting surface of the other block. It is thought that the aerodynamic load acting on the nozzle coupled with possible thermal growth resulted in sufficient rotation of the blocks with respect to the mounting flange to break welds in the attachment lugs and open the cooling water passages. The lugs could be strengthened; however, modifications to accommodate the thermal growth would involve replacing and rewelding water cooling passage plugs.

In the meantime, a set of heat-sink nozzle blocks had been fabricated and the decision was made to proceed with the heat-sink blocks in lieu of the risky modification to the water-cooled blocks. The heat-sink blocks were fabricated from 5-cm thick copper plate and were designed to sustain the maximum HPDE combustor heat load for about 18 to 20 sec without surface melting. Adequate gaps were provided between the four blocks to permit thermal growth without unacceptable stress buildup. Each of the four blocks was attached to a plate, called the heat shield, used with previous water-cooled blocks. A photograph of this nozzle is given in Fig. 60.

Ten burner runs were made with the heat sink blocks. Two small operational problems were experienced during this period. The first involves slight water leaks from the heat shield and the second involves some ablation of the leading edge of the smaller blocks. The leaks result from a slight rotation (under aerodynamic load) of the heat shield which supports the blocks. A stronger, heat-sink nozzle block attachment plate is being designed and fabricated to replace this water-cooled heat shield.

The problem of a slight leading edge ablation occurs primarily on one of the four blocks - the smaller block located on the west side of the burner. This problem developed after six runs with the heat-sink blocks had been made. Once the erosion started (Run 003-011)

Figure 60. Photograph of the HPDE Burner Nozzle

each additional run produced more erosion. This asymmetrical ablation could be indicative of a nonuniform plasma flow; however, inspection of the burner and burner extension liner suggests a mechanical explanation. The copper liner of the burner extension is about 2.5 mm in diam less than the burner liner. The two liners are aligned only on the west side and the flow passes over a forward-facing step about 2.5 mm high in approaching the leading edge of the other blocks. The separated region downstream of this step probably reduces the heating to the leading edge of all blocks except the one on the west side. In addition, the velocity of the flow approaching the side blocks is about 50 percent greater than that approaching the leading edge of the top and bottom blocks, which would also account for increased heating to the side blocks.

A sketch of the side nozzle block is given in Fig. 61 illustrating the loss of heat-sink material near the leading edge and giving some empirical indication of the erosion rate. Photographs of the nozzle section after one run and after three runs are shown in Fig. 62. This nozzle erosion appears to be acceptably small and should cause no problems during powered testing.

4.2 BURNER PLASMA DIAGNOSTICS

4.2.1 Flow Uniformity

A total pressure rake positioned at the exit plane of the nozzle, containing eleven probes on the horizontal and vertical centerlines, Fig. 63, a traversing pitot-static pressure probe, Fig. 51, and static pressure orifices on each of the rectangular nozzle walls at the throat have been utilized to study the plasma uniformity emanating from the burner.

The variation of pitot pressure with time from each of the rake probes is given in Fig. 64 for one typical run. The pressures are relatively steady with time and follow the variation in burner static pressure. The magnitude of the pressure, however, is about 10 percent higher on the two probes on the east side and for the probe at the top of the nozzle. Some transducer swapping was accomplished between runs to provide more complete coverage and summary vertical and horizontal profiles at the nozzle exit are given in Fig. 65, nondimensionalized by the burner pressure, p_s . These data represent an average over a 2- to 3-sec period in the steady-state portion of the run. The apparent nonuniformity in the profile is probably not real but represents a transducer calibration/zero shift problem since the run-to-run variation is greater for all probes which read suspect values.

The traversing pitot-static probe was mounted in the upper wall of the electrode test assembly, a constant-area extension of the nozzle.

Figure 51 Side Nozzle Block Erosion - Heat Sink

a. After One Run

b. After Three Runs

Figure 62. HPDE Heat Sink Nozzle Erosion (Side Block)

a. Schematic of Pitot Rake in Diagnostics Section

b. Photograph of Two of the Four Pitot Rake Segments in Diagnostics Section

c. Locations of Probes

Figure 63. Pitot Pressure Rake Located at Nozzle Exit Plane

Figure 64. Individual Probe Data for Total Pressure Probe during Burner Test M1-002-003.

Figure 65. Summary of Rake Pressure Data

With this probe, the region of apparent pitot pressure nonuniformity could be traversed with a single probe-transducer. On one run, the probe was traversed from the wall to just below the centerline of the nozzle (about 12.7 cm) in about 1 sec. On another run, the probe was positioned on centerline for the 3-sec run duration. Pitot, p_0' , static, p_s , and burner, p_b , pressures are presented as a function of time in Fig. 66 for this nontraversing run.

The probe static and stagnation pressures are presented in Fig. 67 nondimensionalized by the burner static pressure (approximately $0.96 p_0$) as a function of time for the traversing run. Initially, the probe is housed in a small recess in the upper wall and the pitot and static orifices measure about the same pressure, $0.5 p_s$. As the probe traverses from the recess, the pitot orifice immediately measures an impact pressure which is invariant from the wall to the nozzle centerline. This is inconsistent with the pitot rake results and tends to confirm the suspected pitot rake errors. While the pitot pressure is invariant in the survey plane, the static orifice senses a strong variation in static pressure within 3 in. of the wall, starting at about $0.5 p_s$ and increasing to about $0.7 p_s$ on centerline. An approximate measurement of the probe position relative to the centerline is superimposed on the time scale along the abscissa of Fig. 67. The non-dimensional presentation of the pitot-static data was chosen since the sonic location was fixed at the probe and the effective throat area decreased as the probe became progressively exposed. This caused the burner pressure to increase with time and the nozzle static pressure to increase from a sonic value to a subsonic value as also shown in Fig. 67. The sonic pressure based on an isentropic, one-dimensional expansion from the burner is noted in the figure. The Mach number across the survey plane, based on p_s/p_0' , is given in Fig. 68. Each of these measurements reveals a strong difference from simple one-dimensional, isentropic theory. Tests using the traversing static probe without the electrode test assembly, to eliminate the burner pressure sonic area variation, were being initiated when the water pump motor failed. These surveys would have been taken in both the vertical and horizontal planes to confirm these observations.

It has been noted on all HPDE runs that a consistent variation in static pressure existed from wall to wall. These differences were first noted in Ref. 3 and the variation is consistent throughout the run as shown in Fig. 69.

The four static pressures located on each of the nozzle walls at the throat were studied on several subsequent runs to confirm this apparent gradient. The results are shown for eight runs in Fig. 70. Transducers were exchanged between static ports and the entire throat block was rotated 180 deg on selected runs as noted in the figure and, in each case, the static pressure on the sidewalls was significantly less than the pressure on the upper and lower walls. This was true,

Figure 66. Typical Burner Chamber and Traversing Pressure Probe Data with the Traversing Probe on the Flow Centerline

Figure 67. Pressure Temporal Data obtained with the Traversing Pressure Probe Traversing Vertically

Figure 68. Vertical Mach Number Distribution across Nozzle Exit

Figure 69. Variation of Burner Chamber Pressure and Nozzle Wall Static Pressures During Run

Figure 70. Comparison of Measured Nozzle Wall Static Pressures with One-Dimensional Theory

regardless of the presence of the electrode test assembly, independent of the run mass flow, not affected by the presence of seed flow, and not the result of static orifice discontinuities at the surface.

These measurements, along with the pitot-static results, reveal a pressure variation at the nozzle exit plane of up to 25 percent, which is equivalent to a variation in Mach number from about 0.85 to 1.15 across the exit plane.

Extensive computations of this flow field, using an inviscid two-dimensional code developed by Cline (Ref. 4), have been carried out to confirm the apparent flow nonuniformity. This code utilizes a time-dependent solution to treat mixed subsonic and supersonic flows with the governing equations in the hyperbolic form. The steady-state solution is obtained as the asymptotic time-dependent solution for large times. The McCormick finite difference scheme is used to produce fast, accurate solutions for complex two-dimensional flows of mixed types.

The HPDE burner-nozzle was modeled as two two-dimensional flows of the equivalent area ratio (entrance velocity) as shown in Fig. 71. This permitted the development of a quasi-three-dimensional picture of the nozzle exit plane flow by superposition of the two solutions. A plot of the static pressure and Mach number variation across the nozzle exit plane is given in Fig. 72. These computational results predict a strongly accelerated flow near the nozzle wall with a correspondingly reduced static pressure relative to that on centerline. They also predict a more highly accelerated flow near the walls of the side nozzle blocks and a lower pressure on the side than the top and bottom walls. The higher acceleration on the sidewalls appears to result from the significant velocity increase between the start of the top-bottom contractions and the leading edge of the side blocks.

These computed results exhibit a surprising similarity to the experimental results presented previously. The relationship between static pressures on the four walls, Fig. 70, and the variation in static pressure in the one survey plane, Fig. 67, appears to conform well with these quasi-three-dimensional computations.

During actual powered operation of the MHD channel, the nozzle exit (channel entrance) Mach number will be between 0.7 and 0.8 as opposed to the choked ($M = 1.0$) exit in these diagnostics tests. The two-dimensional flow solution was recomputed for this case by simulating a choked throat far downstream of the nozzle exit to reduce the nozzle exit Mach number to 0.8. The gradients were found to be less severe at this slightly reduced nozzle exit velocity as shown in Fig. 73. Even these slight gradients were shown computationally to decay to uniform flow within essentially one nozzle exit height.

a. Two-Dimensional Model - Vertical Plane

b. Two-Dimensional Model - Horizontal Plane

Figure 71. Two-Dimensional Flow Models of HPDE Burner/Nozzle Flow

Figure 72. Quasi-Three-Dimensional Computation of Nozzle Exit Plane Pressure and Mach Number Distribution - Inviscid

Figure 73. Two-Dimensional Computation of Nozzle Exit Plane Pressure and Mach Number Distribution - MHD Channel Operation

Even though the nozzle exit plane flow is highly three-dimensional when choked, as in the burner diagnostic experiments conducted this fiscal year, the flow into the MHD channel will be essentially uniform because the nozzle Mach number will be reduced in the future mode of operation.

4.2.2 Flow Temperature - Enthalpy

The measured oxidizer and fuel mass flow rates into the burner and the burner pressure can be utilized to compute a burner stagnation temperature. From continuity, the flow properties at any station in the burner are related to the total mass flow into the system, $\dot{m}$, as

$$\dot{m} = \rho u A \quad (1)$$

where ρ is the density, u is the velocity, and A is the flow area and for a perfect gas with the Mach number $M = u/\sqrt{\gamma RT}$.

$$\dot{m} = \frac{\sqrt{\gamma} P M A}{\sqrt{RT}} \quad (2)$$

where P is the pressure, T is the temperature, R is the gas constant, and γ is the isentropic exponent. Solving for the static temperature,

$$T = \frac{\gamma}{R} \left(\frac{P M A}{\dot{m}} \right)^2 \quad (3)$$

If the flow is isentropically stagnated, the stagnation temperature is given as

$$T_0 = \frac{\gamma}{R} \left(1 + \frac{\gamma - 1}{2} M^2 \right) \left(\frac{P M A}{\dot{m}} \right)^2 \quad (4)$$

Hence, the stagnation temperature of the flow can be computed from the measured mass flow and pressure at any location in the burner system with input values of γ , A , and M . This formulation of stagnation temperature is analogous to that used in the computation of the characteristic velocity, C^* , by others (Ref. 5). Values for γ and R can be established with precision for the HPDE burner plasma. The Mach number, pressure, and area, however, vary through the burner system.

Static pressures measured on the wall are available in the burner chamber and at the nozzle throat. As indicated, the flow at the throat is highly three-dimensional and an accurate solution of Eq. (4) involves the use of average properties at any cross section. Uncertainties are involved in the computation of the average pressure, Mach number, and the effective flow area at any station.

The effective flow area at the throat plane was computed using a two-dimensional boundary-layer program following the method of Patankar and Spaulding (Ref. 6) with modifications (Ref. 7). The effects of heat transfer at the burner wall and the pressure gradient through the nozzle are included. The results of the boundary-layer computation are given in Fig. 74. It can be seen that the displacement thickness at the throat ($x = 0.9$ m) has a slightly negative value of -0.25 mm. Hence, the effective flow area at the throat is only negligibly different from the physical throat area. Similarly, the flow area at the burner pressure orifice location is only negligibly different from the physical area.

The stagnation temperature has been computed for several runs using the nozzle static and burner chamber pressures in Eq. (4) with additional considerations as outlined above. Since the flow at the nozzle exit is highly three-dimensional, the mean static pressure acting across the exit plane is not directly related to the wall static pressure actually measured and inconsistent stagnation temperatures were computed. Primary reliance has been placed on the determination of stagnation temperature based on the burner pressure.

The experimental stagnation temperature based on the burner pressure is ratioed to the theoretical stagnation temperature in Fig. 75. The ratio of the experimental temperature to the theoretical temperature ranges between 0.93 and 1.03 for all runs. For conditions near stoichiometric without seed, the ratio is approximately 0.96. The last three seeded runs exhibit a reduced experimental temperature ($T_{\text{exp}}/T_{\text{th}} \approx 0.93$) which is probably indicative of the seed precipitation problem discussed in Section 4.1.3. The temperature ratio is also given for stoichiometric and off-stoichiometric conditions in Fig. 76.

The feasibility of using an emission/absorption (ϵ - α) scheme for determining H_2O and CO_2 specie concentrations and gas static temperatures in the HPDE combustor flow was studied on two runs. The measurements were obtained using the Circular Variable Filter (CVF) Spectrometer (Fig. 77), which employs a liquid-nitrogen-cooled InSb detector that responds in the near-infrared wavelength region from 2 to 6 μm . A Barnes 1,000°C Blackbody was used as the source, and 15.25- by 1.27-cm circular IRTAN II (ZnS) flats were used as windows.

The details of the ϵ - α method have been reported in Ref. 8. In general, the technique requires the measurement of radiance and transmittance at a fixed wavelength in an IR band through several adjacent paths

Figure 74. Two-Dimensional Flow Boundary Layer and Displacement Thicknesses in HPDE Burner/Nozzle

Figure 75. Comparison of Theoretical and Experimentally Determined Burner Stagnation Temperatures

Figure 76. Comparison of Theoretical and Experimental Plasma Temperatures in the Burner for Various Oxygen-Fuel Ratios

Figure 77. The CVF Spectrometer

in a cylindrically symmetric source. The data, in conjunction with the appropriate radiative band model formulation, are then used with an iterative numerical technique to obtain the radial profiles of both temperature and concentration. In the case of the HPDE diagnostics, no spatial scanning of the flowing gas was attempted, hence the ϵ - α technique was intended to be used to determine homogeneous temperature and specie concentrations.

The instrumentation arrangement for the HPDE optical study for the first run is shown in Fig. 78. Water leakage from the nozzle block assembly resulted in some moisture accumulation on the lower window which had to be cleared before the run. The apparatus was changed so that it viewed the channel horizontally for the second run.

The optical equipment was mounted on the concrete floor of the HTL building, and the vibration levels encountered during the burner operation were minimal.

Figures 79 to 84 show the data obtained for the prerun calibration, Runs 1 and 2 emission, Runs 1 and 2 absorption, and the postcal. The filter wheel covers two distinct wavelength regions which have different transmission curves in the overlapping region. The ordinate scales are arbitrary; the emission intensity is roughly seven times the precal signal. The emission data from the two runs were ratioed to their respective calibration signals and multiplied times the source spectral radiance to obtain the spectral radiance of the combustion process. This resultant spectral radiance was then used with standard blackbody tables to determine the corresponding temperature. The emissivity was assumed to be equal to one. The spectral radiance and temperature are presented in Table 10 for both runs. The flow temperature at the point of measurement was computed from the theoretical flame temperature and a quasi-three-dimensional nozzle expansion as shown in Fig. 85. It can be seen that the agreement between the measured and theoretical temperatures at the point of measurement is poor.

There were two major problems in the analysis of the absorption signal for both runs. First, the absorption calibration signal was lost during the prefire oxidizer flow, and did not return to its original level until several minutes after the test. Second, the emission signal is approximately twenty-five times greater than the absorption

Table 10
Spectral Radiance and Temperature obtained from
CVF Spectrometer

λ , μm	Run 1		Run 2	
	Spectral Radiance	Temp, $^{\circ}\text{K}$	Spectral Radiance	Temp, $^{\circ}\text{K}$
4.6	4.00×10^4	3,470	3.30×10^4	3,350
4.8	3.24×10^4	3,360	3.18×10^4	3,280

Figure 80. Emission Results for First Run of Infrared Emission-Absorption Experiment

Figure 81. Absorption Results for First Run of Infrared Emission-Absorption Experiment

Figure 84. Absorption Results for Second Run of Infrared Emission-Absorption Experiment

Figure 85. Calculated Flow Geometries and Properties for the Infrared Emission-Absorption Experiment

Figure 87. Heat Stored in Simulated Electrode Wall after a 3.0-sec Burner Firing

Figure 88. Comparison of Calculated and Measured Temperature Distribution in Simulated Electrode Wall during Thermal Soak after a 13.4-sec Firing

Figure 89. Heat Stored in Simulated Electrode Wall after a 13.4-sec Burner Firing

orientation of this anisotropic material. Refinement of the theoretical model to account for these variables is not warranted and the thermocouples embedded in the actual HPDE generator wall will be used to further monitor these phenomena.

The erosion of the graphite caps in the electrode test assembly was found to be small. The caps near the entrance of the electrode test assembly exhibited some rounding along their leading edge. A photograph of the simulated electrode wall after the six runs totaling about 35 sec of operation at surface temperatures from 980°K to 1,300°K is given in Fig. 90. The surface erosion was less than 0.127 mm after this exposure, except for the leading edge regions. As evidenced in the photograph, loss of the MgO refractory occurred during the course of some of the run. Since the refractory is not sheared off, as shown in the figure, it is speculated that the expulsion results from residual moisture and occurs during the soak period after the burner is shut off. In the case of the generator channel, the refractory is deeper and extra precautions will be taken to dry and cure the material. As a consequence significant refractory loss is not anticipated nor inferable from the results of these tests.

4.4 PRELIMINARY TEST PLANS FOR THE MAGNET AND GENERATOR CHANNEL

Considerable design effort has been expended in the formulation of the HPDE to provide a meaningful demonstration of MHD power generation performance under conditions emulating coal combustion. The scale of the experiment represents a reasonable compromise between the cost on the one hand and a scale suitable for extrapolation with confidence of the design data available from the completed facility on the other. Since the design was based on current technology at its conception (1974), elements of risk are inherent to the mechanical design of the magnet and generator channel as well. In the case of the magnet, considerable effort has been expended in the structural analysis of the FCS designed to react the tremendous Lorentz forces on the magnet coils at the 6 T field strength. Structural refinements have been adopted; however, the FCS remains risky in the sense that it has a recognized finite life insofar as 6 T pulsed operation is concerned. In the case of the generator channel, electrical insulation gaps in the electrode and insulating pegwalls have been designed for peak Hall and Faraday electric field strengths of 2,000 and 4,000 v/m, respectively. For the design operating point with a 6 T field, the electrode gap potential is 35 v and the interpeg voltages can be in the 60 to 120 v range. In view of the high surface temperatures anticipated on the electrodes, neither of the gap voltages is conservative and the potential for breakdown is present.

Recognizing these risk areas, it has been decided to perform the initial performance testing at a reduced field strength of 4 T. The 6 T generator channel performance can be inferred at any lesser field provided

Figure 90. Photograph of Electrode Caps after Six Runs Totaling 36 sec of Operation

that the product of the plasma electrical conductivity and the square of the magnetic field strength (σB^2) is maintained at its design value. (In a high subsonic generator design such as the HPDE, the plasma velocity is essentially constant and need not be considered.) The practical upper limit for conductivity in the HPDE is 30 to 40 mho/m, therefore, a field of 3 to 4 T is required for simulation. With a 6 T field, the generator channel is designed to operate with a plasma conductivity of approximately 9.5 mho/m at its inlet. At 4 T, simulation would require an inlet value of conductivity of approximately 20 mho/m. Such a value can be achieved by increasing the seeding rate, using a cesium based seed, and reducing the N_2-O_2 ratio of the oxidizer.

Table 11 summarizes the variations of principal experimental parameters of a 4 T test in comparison with the 6 T design case. As indicated in the table, the electric fields are proportional to the magnetic field strength and for 4 T operation would be approximately 67 percent of the design limiting value. The increased current density and reduced field strength in the channel at 4 T operation would result in an increased electrode voltage drop relative to the Faraday voltage which, in turn, results in a slightly reduced power production. The increased current and reduced voltage to the load bank would also be contrary to its nominal design; however, the required reduction in resistance can be readily achieved. The wiring from the channel is designed to insulate to 20 kv and the only potential problem is in the increased current. The current rating of the wire is based on the conductor temperature rise which is proportional to the square of the current. Thus, for run time of approximately 7 sec, it will be no more severe than the nominal designed case and sufficient margin is available to exceed this value.

In the case of the magnet, the forces generated in the coils are proportional to the square of the field strength and at 4 T will be approximately 45 percent of the design value. This has an obvious advantage, however, the magnet has been designed to operate at only two field strengths corresponding to two electrical connections with the available power supply. With the coils in parallel and water cooled, the magnet will operate continuously with a peak field strength of 3.5 T. With the coils series-connected and cooled to LN_2 temperature, the magnet will be pulsed to 6 T with a test window of approximately 40 sec during which the field remains above 6 T. To test the magnet, at fields greater than 3.5 T, it is sufficient to proceed as if a 6 T pulse were going to be performed and switch off the power supply when the desired field strength is reached. This is insufficient in the case of generator testing, since no "window" or period at near constant field is produced in which to test the channel. To provide the necessary test window at reduced fields, it is necessary to incorporate a current-limiting device in the present power supply. The simplest device would be a series-connected resistor which unfortunately could complicate the fault protection.

Table 11

Comparison of 4-Tesla Test Parameters with Nominal Design Values

	% DESIGN VALUE	COMMENTS
MAGNET Voltage Current Forces Power	67% 67% 44% 45%	Requires off-design power supply operation
BURNER Mass Flow Seed Rate Pressure Temperature	No change 150% No change 103% ($\sim 100^\circ\text{K}$)	Higher seed rate and/or lower N_2/O_2 ratio required.
CHANNEL Pressure Power Heat Transfer Current Density Electric Fields	No change 90% 110% 150% 67%	Possible higher heating rate, higher electrode voltage drops relative to induced field
DIFFUSER Recovery Heat Transfer	No change 110%	No apparent problems
LOAD BANK Voltage Current Load Resistance	67% 150% 46%	May require more parallel loads, wiring OK

Instead, it is proposed to use existing reactors in the primary side of the power supply transformer to reduce the power supply output.

Based on these considerations, a test plan comprised of six phases for the magnet and channel is as follows:

Phase I Warm Mode Magnet Checkout

After final placement of the magnet, but before installation of the vapor barrier, the coils will be tested for ground fault, and the magnet energized to approximately 2 T in a heat sinking mode. The magnet characteristic inductance prior to saturation of the steel will be determined.

Phase II Cold Mode System Check

The magnet will be cooled down and pulsed in steps of 0.5 T from 3.0 T to a maximum of 4.5 T. The pulse tests at 3.5, 4.0, and 4.5 T will be repeated with burner operation at the field peak to ensure control functioning in the presence of the magnetic fringe field. Six unseeded firings of the burner with the channel attached and the magnet unpowered will be made to cure the refractory in the channel and calibrate the channel thermal response.

Phase III Performance Demonstration at 4 T

Twenty firings of the HPDE system will be performed in the Faraday configuration. The first eight firings will be used to optimize the system parameters for performance demonstration. The last twelve runs will be used to establish the channel voltage-current characteristics over a systematic variation in important control parameters (seed rate, load distribution, etc.).

Phase IV 6 T Magnet Checkout

This will be repetition of Phase II with the magnet ultimately pulsed to 6 T. The 6 T pulse will be followed by a repeat with live burner firings. The dummy channel may be installed in the magnet for this phase.

Phase V High Field Performance Demonstration

Eighteen powered runs will be made. The initial thirteen runs will be with increasing field strength starting at 5.0 T and optimizing at 6.0 T. The last five runs will be used to establish the channel V-I characteristics at 6.0 T in the Faraday configuration.

Phase VI Diagonal Operation of Channel

The channel loading will be reconfigured and twelve firings with 6 T field pulses will be made to demonstrate diagonal mode design performance.

Only the first three phases will be completed in FY79, with the last three being performed in FY80.

The test plan outlined above achieves performance demonstration early and with minimum risk to the hardware. To achieve this, a minor simulation penalty is paid in that enhanced plasma conductivity is required. The plan is designed to provide for independent magnet, burner, and channel checkout at new test conditions with an attempt to minimize risk to the facility. Of the fifty total powered runs, twenty-two will be at the full 6 T field capability of the magnet which will leave a substantial life to the magnet structure based on the design calculations.

V. PROGRAM STATUS SUMMARY

For the reporting period covered, the major efforts have been expended in completing the generator channel assembly, completing the burner testing, and continuing the assembly of the magnet. Considerable effort was devoted to refinement of the design of the magnet cooling system and analytical studies performed to provide a guideline for the cooldown procedure. The water-cooled nozzle blocks continued to present problems during burner operation which were alleviated with the heat sinked design. Loss of the high pressure water pump drive motor necessitated an early termination of the burner testing; however, the results obtained to date do not indicate any significant nonuniformities or anomalous aerodynamic characteristics in the plasma.

The program schedule is given in Fig. 91, and the status of the indicated items at the close of the reporting period is described in the following:

1. Facility Development

This effort was completed in FY77 and no further effort was scheduled nor required in the current reporting period.

2. Flow Components

Fabrication of a set of heat-sinked nozzle blocks was completed. Fabrication of the diffuser was started on schedule and was slightly ahead of schedule at the close of the period, being approximately 60 percent complete.

△ SUBEFFORT BEGINNING

▽ SUBEFFORT COMPLETION

SOLID BAR INDICATES WORK ACCOMPLISHED

BROKEN BAR INDICATES SLIPPAGE AND RESCHEDULING OF ACTIVITIES

HPDE PROGRAM SCHEDULE

REVISED 7-7-78

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Figure 91. HPDE Program Schedule

3. Generator System

The assembly and testing of the individual generator sections were completed approximately two weeks ahead of schedule. At the close of the period, alignment and interconnecting the channel sections on the channel cart were in progress. Instrumentation, primarily electrode voltage and current measurements, was defined and the required materials procured. At the close of the period, the installation of this instrumentation and wiring was on schedule.

4. Magnet Systems

The coil in its entirety and approximately 95 percent of the coil FCS were installed on the magnet. During the installation of the FCS, strain gages and thermocouples were installed on many of the members prior to their placement. The installation of the FCS is about 4 weeks behind schedule at the close of the period. Due to the desire to mount the strain gages and thermocouples just prior to the installation on the magnet to minimize potential for damage, the instrumentation installation has been allowed to slip. This will not have an impact on the schedule. Similarly, completion of the magnet power supply bus work has slipped, primarily due to material shortage and procurement delay and will not impact the magnet completion schedule. The fabrication of the panels for the vapor barrier has been completed considerably ahead of schedule. The principal problem encountered has been the vacuum box, which did not meet specifications after its initial fabrication and subsequent attempts at repair. It appears that an alternative insulation system based on styrofoam has a higher probability of performance in comparison with the vacuum box and designs based on this alternative are being completed.

5. Shakedown and Operation

Burner testing was performed during the year with a total of fourteen successful runs and a cumulative time of 113 sec. The water-cooled nozzle blocks continued to be a problem with regard to leaks and deflections. The heat-sinked nozzle blocks eliminated these problems and were used on 10 of 14 successful burner firings. Various probes were used in the diagnostics section and extensive pressure data have been analyzed to confirm the flow uniformity. Wall calorimetry measurements indicate that the channel design heating rates are reasonable and the graphite cap erosion is minimal. The infrared emission-absorption experiment was partially successful, the conductivity and enthalpy measurements were unsuccessful. The high pressure water pump failed prior to the last few scheduled runs and the burner test was terminated in order to not impact the magnet assembly schedule.

Preliminary planning for the magnet checkout and system checkout with performance demonstration at a reduced field of 4 T is in progress.

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