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QUARTERLY TECHNICAL PROGRESS REPORT

July—September 1982

December 1983
Date Submitted

Work Performed Under Contract No. AC02-79ET10815

The University of Tennessee Space Institute
Tullahoma, Tennessee

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Distribution Categories UC-90g and UC-93

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JULY-SEPTEMBER 1982

Prepared by:

The University of Tennessee Space Institute
Energy Conversion Research and Development Programs
Tullahoma, Tennessee 37388

Date Submitted - December 1983

PREPARED FOR THE UNITED STATES
DEPARTMENT OF ENERGY

Under Contract No. DE-AC02-79ET10815

ABSTRACT

In Task 2, detail design work was resumed on the superheater. Satisfactory bids were received for the air heater and lowest price responsive bidder was chosen.

In Task 3, the primary activity reported is the conduct of three tests in the LMF1C series. The status of the environmental monitoring program is reviewed.

In Task 4, preliminary analyses of the test data from the three tests conducted during the quarter are included. The heat fluxes, combustor pressure and combustor efficiencies are reported.

The performance of the nozzle, diagnostic (Hall) channel and diffuser is compared with an analytical model for each test run. The performance of the new diffuser which was installed during the quarter is discussed. The test results from the downstream components; i.e. slag screen, radiant furnace, secondary combustor and materials test module, are discussed. Slag removal from the radiant furnace, refractory performance and metals performance is covered. A summary report is included on the results of the cold flow modeling of the secondary combustor, which involved variations in relative velocity of the secondary air, the angle of injection and the flow constriction.

Diagnostic support of testing activities is described, including the use of the laser doppler velocimeter (LDV) for the secondary combustor modeling. Luminosity and line reversal temperature measurements were made in support of the CFFF tests. A photodiode line reversal system has been designed which has the promise of being more reliable, easier to install on operational equipment and cheaper.

CONTENTS

Sections

I. OBJECTIVE AND SCOPE OF WORK	1
II. SUMMARY OF TECHNICAL PROGRESS	3
A. <u>TASK 2 - DESIGN AND FABRICATION OF THE EIGHT LB/S</u> <u>TEST EQUIPMENT</u>	3
2.3 Downstream Components	3
B. <u>TASK 3 - BASE OPERATIONS FOR THE CFFF</u>	3
3.1 Laboratory Operations	3
C. <u>TASK 4 - EIGHT LB/S HIGH SLAG THROUGHPUT TEST</u>	5
4.1 Primary and Secondary Combustors	5
4.2 MHD Power Train	18
4.3 Downstream Components	54
4.6 Diagnostic Support	74
D. <u>TASK 5 - TESTING OF DOE SUPPLIED COMPONENTS</u>	78
E. <u>TASK 6 - MODIFICATIONS TO THE CFFF</u>	78
F. <u>TASK 7 - TECHNOLOGY DEVELOPMENT PROGRAM</u>	78
References	81
Topical Report Status	82
Appendix A	A-1

FIGURES

1. Heat Flux Distribution Throughout the Combustor System for Constant Stoichiometry and N/O Ratio	9
2. The Effect of N/O Ratio on the Heat Flux Distribution Throughout the Combustor System for a Stoichiometry of 0.83	10
3. The Effect of N/O Ratio on the Heat Flux Distribution Throughout the Combustor System for a Stoichiometry of 0.92	11
4. Heat Flux Distribution Throughout Combustor System	12
5. Cumulative Heat Loss from Combustor System Showing Constant Stoichiometry and N/O Ratio	13
6. Cumulative Heat Loss from Combustor System Showing Effect of Variable N/O Ratio	14
7. Cumulative Heat Loss from Combustor System Showing Variable N/O Ratio, Constant Stoichiometry	15
8. Cumulative Heat Loss from Combustor System Effect of Variable Stoichiometry	16
9. LMF1 Combustor Heat Loss	17
10. Experimental and Theoretical Combustor Pressure versus Total Mass Flow Rate	19
11. Vitiation Heater Radial Temperature Distribution, Case: LMF1C-2	20
12. Summary of LMF1C Operating Conditions	23
13. Adiabatic Flame Temperature Variation for LMF1 Flow Train	25
14. Combustor and Nozzle Heat Loss	26
15. Channel Heat Loss	27
16. Diffuser Heat Loss	28
17. Total Upstream Heat Recovery	30
18. Surface Control Volume for Heat Transfer Modeling	34
19. Typical Boundary Layer Shock Wave Interaction	37
20. Representation of 1-D Modeling of the Shock Zone	39
21. LMF1 Static Pressure Distributions	41
22. LMF1 Heat Flux Distributions	42

23.	LMF1 Gas Dynamic Distributions.	45
24.	Cumulative Heat Loss from Channel and Diffuser versus Theoretical Predictions	46
25.	Comparison of LMF1 Diffuser Designs, Demonstrated Pressure Distributions	47
26.	Approximate Shock Location for the Two Diffuser Configurations for LMF1A and LMF1C Test Series Data	49
27.	Schematic of Power Supply and Instrumentation for Measurement of Plasma Conductivity During LMF1C	50
28.	Pretest Measurements of Conductivity Channel Interframe Resistance with Nominal 5 Ω Interframe Resistors Installed and Voltage Applied at Frame 14	52
29.	Slag Tap Diverted Hot Gas Flow	55
30.	Particulate Size from Cross-over Duct during Tests LMF1C-3 and LMF1C-4	58
31.	LMF1C-2 Flow Train	59
32.	NO _x and Temperature along length of LMF1 Flow Train during LMF1C-2 Test	60
33.	LMF1C-2 NO _x Levels	61
34.	NO _x and Temperature along LMF1 Flow Train during LMF1C-4 Test . .	62
35.	Alumina-Chrome Oxide Bricks Surrounded by Alumina-Silicon Carbide Plastic Refractory on Primary Slag Screen	64
36.	Insulating Castable Refractory on Material Test Door in Drum Module after LMF1C-4	65
37.	LMF1C-2 Upstream MTM Tube Appearance	68
38.	LMF1C-2 Downstream MTM Tube Appearance	68
39.	Cross-Section of Ash/Seed Deposit, Front Side, Tube A-10, LMF1C-2	70
40.	Tube-Derived Metal Oxide/Sulfide Phases in Inconel 600 Ash/Seed Deposit	71
41.	Fly Ash and Seed on Front Deposit Surface, LMF1C-1.	72
42.	K ₂ SO ₄ Back Side Deposit, LMF1C-1.	72
43.	Temperature Profiles Across LMF Nozzle Throat	75

44.	Temperature Profile Ahead of Slag Screen for LMF1 Base Conditions	76
45.	Relative Spectral Transmission of Gas at the Diffuser Exit During LMF1C-3	77
46.	LV Optical Train for the Primary Combustor Flow Study	80

TABLES

I.	Planned Nominal Test Conditions	6
II.	Combustor Test Results	7
III.	Summary of LMF1 Test Points	21
IV.	Interframe Resistances Measured During LMF1C-3 in Ohms	53
V.	Air Heater Test Materials	67
VI.	Typical Laser Velocimeter Measurement Confidence Tolerances	79

SECTION I

OBJECTIVE AND SCOPE OF WORK

Under Contract No. DE-AC02-79ET10815, the overall objective is to advance the technology of direct coal fired MHD components and systems required for MHD power systems operating under conditions simulating those of central power stations.

The specific objectives of the UTSI, using the DOE Coal Fired Flow Facility, are to resolve experimentally and analytically the key technical problems which have been identified or which may be found to occur in direct coal fired MHD systems with moderate to high ash carryover. The problem areas involve (1) combustor performance, (2) seed separation efficiency from the exhaust gas stream, (3) MHD generator performance and design requirements using slag deposition on the channel walls to protect the walls from the intense thermal environment, (4) effects of plugging, fouling and corrosion by solid slag particles during normal operation, (5) response of candidate materials to direct coal fired MHD environment and (6) operating procedures and equipment to meet pollution requirements.

The overall scope of work contains eight tasks summarized under each of the following task headings.

1. Task 2 - Design and Fabrication of the 8 lb/sec Total Gas Flow, High Slag Throughput Test Equipment

Design, procure, fabricate and install the 8 lb/sec (3.63 kg/s) total mass flow test equipment in the Coal Fired Flow Facility, providing DOE with a schedule and cost estimate for each activity. The design goal for all components is the demonstration of predicted system characteristics required of an MHD system (e.g. life, performance, reliability, failure mode, etc.) and determining applicability of design for base level operations.

2. Task 3 - Base Operations for the CFFF and ECF

Perform all operations and maintenance required to maintain the CFFF and the ECF in a standby condition. Items included are a) materials and components handling equipment, b) required equipment for installation of components, coal/seed handlings, etc., c) emissions monitoring, and d) instrumentation and sampling devices for continuous monitoring of O₂, CO₂, NO_x, CO, SO₂, H₂S, etc. This Task covers such major services as:

- 1) General Administration and support
- 2) Operations planning
- 3) Crew and staff maintenance
- 4) Grounds and building maintenance
- 5) DOE vendor occupancy
- 6) Facility design maintenance

3. Task 4 - 8 lb/sec Total Gas Flow High Slag Throughput Test

Operate the Coal Fired Flow Facility to progressively test and demonstrate that equipment and subsystem requirements for UTSI-designed components (Task 2) are met via Operation, Performance Demonstration and Acceptance Tests. Based on acceptance test results, engineering data are to be obtained toward the development of scaled-up MHD systems.

4. Task 5 - Testing of DOE Supplied Components

Utilize the Coal Fired Flow Facility for purposes beyond the demonstration of the 8 lb/sec flow train and superconducting magnet flow train. Specifically, UTSI is to be the initial operations contractor in performing feasibility studies for testing components in the CFFF, the components being supplied by other Department of Energy contractors via DOE.

5. Task 6 - Modifications to the CFFF

Provide modification to the CFFF as required to test and to provide development assistance for DOE-supplied components, equipment and subsystems.

6. Task 8 - Test Integration and Interface

Provide technical and management expertise to DOE upon request to attend meetings and conferences and serve on panels relating to MHD power generation. Also, at DOE request, UTSI is to perform MHD related studies for outside contractors, and to aid component vendors in their development of components to be tested in the Coal Fired Flow Facility.

7. Task 9 - Program Management

Provide for effective project management by utilizing and updating the presently available PERT and MIS system activities for the total program. These systems identify firmly established baselines for each task. Project management is to be effected by close alignment of the UTSI reporting systems with the DOE Uniform Contract Reporting System (DOE/CR-0001/1) and the proposed MHD Division Work Breakdown Structure (WBS).

SECTION II

SUMMARY OF TECHNICAL PROGRESS

A. TASK 2 - DESIGN AND FABRICATION OF THE EIGHT LB/SEC TEST EQUIPMENT

2.3 Downstream Components

Work on the Superheater Test Module (SHTM) was suspended during July and August because of higher priority projects assigned to the Design Engineering section. Efforts were reinitiated during September on detail drawings of the test and temperature control modules and the transition and elbow sections between the modules. A review of the layout of the SHTM and how it should fit into the existing Coal Fired Flow Facility (CFFF) flow train was started along with a review of the support structure. At the end of the quarter, a final layout had not been approved.

A briefing for all Energy Conversion Research and Development Program (ECDP) personnel was held to acquaint them with the purpose, operation, and location of the SHTM so that potential problems might be identified and solved.

After contacts had been made with potential air heater (AH) vendors and their comments were incorporated into the technical specifications, a request for quotation was sent to the 19 vendors who had expressed an interest in bidding. Of this number, ten responded with bids. These bids were evaluated and Energy Conversion Technology, Inc. of Pittsburgh, Pennsylvania was selected as the vendor who proposed the lowest cost AH which met the technical specifications.

Future actions will include a visit to Energy Conversion Technology to assess their ability to produce the AH and submittal of this vendor and design to DOE for approval. Because of uncertainties in funding and program guidance, the purchase order for the AH may not be released until the first week in January.

2.5 Pollution Control

The design criteria for the baghouse and electrostatic precipitator (ESP) were prepared and approved internally. A literature search was begun to assemble available data that may be applicable to application of these particulate control devices in an MHD system.

B. TASK 3 - BASE OPERATIONS FOR THE CFFF

3.1 Laboratory Operations

During the quarter all testing activity occurred in the CFFF. Three tests of the LMF1C series were successfully completed. These plus the LMF1C-1 tests conducted last quarter represented a total operational time

of 25.1 hours with 10.8 hours on coal. Total accumulated test operational time for the test train at the end of the period was 76.26 hours of which 29.4 hours was operation on coal.

The new diffuser, designed with an abrupt area change or "sudden expansion" rather than a continuous area change, was installed and successfully operated during the period. Fabrication of the flow channel for the Heat Recovery Seed Recovery (HRSR) tests was started.

No further problems with the induced draft fan were encountered during the period. Numerous minor operational problems were cleared as part of test preparations.

The performance of the gas sampling systems improved, although the sampling environment, consisting of fly ash and especially potassium compounds, continued to present problems in reliably acquiring a clean, dry sample. The automatic purge cycle has been helpful, but it is necessary in certain severe environments, such as the diffuser, to manually clean the probe during operation.

Other activities in the environmental and chemical laboratories were devoted to support of the three tests.

Raw pulverized coal samples were analyzed for moisture and seed content prior to all testing in the CFFF. A variety of analyses of slag were completed. The laboratory analyzed a total of 26 pulverized seeded coal samples and 18 slag samples from the CFFF. The pH of the cyclone water was determined before and after each test in the CFFF. Samples from each fuel oil shipment were analyzed for carbon, hydrogen and nitrogen.

Scanning Electron Microscope (SEM) analyses for this quarter included: Samples from M.T.M. tube B10, coal and particulate, each of which were analyzed for elemental and some layering characteristics. Elemental analyses of some samples were compared using the SEM and the Atomic Absorption Spectrophotometer.

The laboratory analyzed 19 samples of water during this quarter. Each sample was subjected to 28 different analyses. Details and interpretation of the analyses are presented in the appropriate sections of this report.

For the coming quarter (October-December 1982) the analytical technique development will focus heavily on methods of analysis for potassium carbonate in slag. An energy symposium sponsored by Leco Corporation was attended in Nashville, Tennessee October 13 and 14, 1982.

During the quarter the Environmental Group participated in two ambient air audits, one by Multitech from Montana and one by the State of Tennessee. While the ambient air monitoring trailers still are presenting some operational problems, the audits indicated a significant improvement from the previous audit. All three trailers are now fully operational. One NO_x analyzer at the base site and an SO₂ analyzer at the Impact I Site were outside calibration specifications.

1

Terrestrial studies have continued with complications resulting from a power line being placed through one of the terrestrial sites near the Impact I ambient air trailer. Another site was selected and samples will be collected from the new site, which is located SE of the CFFF near the observatory.

The Environmental Plan was completed and a draft of the Baseline Ambient Air Report was prepared.

Quality Assurance and Facility Lab personnel are continuing actions necessary for ASME code certification.

Facility improvements completed during the quarter included:

- 1) Installed Halon fire suppression systems in gas sampling trailer and van.
- 2) Performed PCB certification tests on four transformers and four circuit breakers in CFFF main substation (in conjunction with annual maintenance/inspection of CFFF substation).
- 3) Converted gas space heaters in CFFF test building and welding shelter over to electric pilot lights.

C. TASK 4 - EIGHT LB/SEC HIGH SLAG THROUGHPUT TEST

4.1 Primary Combustor

The LMF1 test series at present has accumulated about 21 hours of coal combustion. The primary objective of the LMF1C testing is the evaluation of system control of NO_x , SO_x and particulates. The combustor has operated on coal for about 15 hours during the LMF1C testing. The LMF0 test series had demonstrated that the NO_x concentration profile strongly depends upon stoichiometry, N/O ratio and total fuel thermal input. Thus, LMF1C was selected to contain a base case and several parametric variations. The nominal flow conditions scheduled during this portion of the LMF1C series are summarized in Table I. The flows listed included the fuel and oxidant flows to the vitiation heater and the combustor.

Because of difficulties encountered in maintaining stable flows, nominal conditions were not obtained consistently. Therefore, the data reported here represent the average of the conditions achieved for each case. Test results from the 13 cases analyzed are summarized in Table II.

EVALUATION OF COMBUSTOR PERFORMANCE

Experimental data from the LMF1C tests conducted to date are treated in a similar fashion of that described in previous reports, that is, the combustor performance will be evaluated in terms of heat transfer characteristics and relative combustion efficiency.

TABLE I
PLANNED NOMINAL TEST CONDITIONS

CASES LMF1-*	THERMAL INPUT (MW)	FLOWS (PPS)						N/O	STOICH
		BURNER		COMBUSTOR		TOTAL			
		NITROGEN	OXYGEN	OIL	OIL	COAL+SEED	TOTAL		
C1(5), C2(5) C3(5)	23.2	1.240	3.11	0.096	0.407	1.14	5.993	0.40	0.83
C2(6)	23.2	1.24	3.11	0.116	0.387	1.14	5.993	0.40	0.83
C3(6)	23.2	1.38	3.45	0.106	0.397	1.14	6.473	0.40	0.92
C3(9), C1(6)	23.2	2.18	3.11	0.116	0.387	1.14	6.933	0.70	0.83
C3(7)	23.2	2.42	3.45	0.129	0.374	1.14	7.513	0.70	0.92
C4(5), C3(8)	23.2	3.13	3.13	0.138	0.365	1.14	7.903	1.00	0.83
C4(6)	23.2	3.36	3.36	0.148	0.355	1.14	8.363	1.00	0.90
C4(7)	27.9	2.68	3.83	0.139	0.466	1.37	8.485	0.70	0.85
C4(8)	27.9	2.90	4.14	0.150	0.455	1.37	9.015	0.70	0.92

*Numbers in parentheses correspond to steps as listed in the individual run schedule for given test.

TABLE II
COMBUSTOR TEST RESULTS

CASE	NOMINAL		TOTAL FLOW (PPS)	COMBUSTOR PRESSURE (PSIA)	ADIABATIC FLAME TEMP. (K)	SYSTEM HEAT LOSS (MW)	% OF THERMAL INPUT
	N/O	STOICH					
LMF1C-1 (5)	0.40	0.83	6.02	71.40	3152	2.06	8.87
LMF1C-2 (5)	0.40	0.83	6.09	73.10	3101	2.12	9.13
LMF1C-2 (6)	0.40	0.83	6.11	71.60	3115	2.35	10.12
LMF1C-3 (5)	0.40	0.83	5.89	67.90	3128	2.40	10.34
LMF1C-3 (6)	0.40	0.92	6.31	74.80	3123	2.64	11.37
LMF1C-1 (6)	0.70	0.83	7.02	75.10	3030	1.75	7.54
LMF1C-3 (9)	0.70	0.83	6.98	81.20	3040	2.14	9.22
LMF1C-4 (7)	0.70	0.85	8.32	78.10	3032	2.31	8.27
LMF1C-3 (7)	0.70	0.92	7.39	84.70	3032	2.26	9.74
LMF1C-4 (8)	0.70	0.92	9.12	102.80	3057	2.53	9.06
LMF1C-3 (8)	1.00	0.83	7.81	87.40	2957	1.83	7.88
LMF1C-4 (5)	1.00	0.83	8.03	90.60	2936	1.92	8.27
LMF1C-4 (6)	1.00	0.90	8.43	94.90	2949	1.96	8.44

For this series, test variables were the fuel thermal input, (oxidant flow rate and the combustion stoichiometry). In two tests cases the thermal input was 27.9 MW and for the remaining cases it was 23.2 MW. The total mass flow rate varied between 2.73 to 4.09 kg/s (6 to 9 lb/s) resulting in combustor pressures between 5 and 7 atmospheres. Test conditions provided stoichiometric ratios from 0.83 to 0.92 and N/O ratios of 0.40, 0.70 and 1.00.

COMBUSTOR HEAT TRANSFER MEASUREMENT

Components of the LMF combustor system discussed here include the burner plate (BP), vitiation heater (VH), coal tube (CT), disperser plate (DP) and combustor (a 14.4" long cylindrical section plus a 18.1" long conical section). Separate cooling circuits provide cooling water to each component and individual cooling tubes of the combustor sections. The water lines are arranged so that separate heat loss measurements are made axially along the combustor. The cooling water flow rate and temperature rise are recorded for each circuit to permit calculation of heat rejection as well as average heat flux rates during the test run.

Plots of typical heat flux levels throughout the combustor system are shown in Figures 1 through 4. The general tendency of the heat flux is to rapidly increase through the cylindrical and conical combustor section, and reach a maximum in the nozzle throat. This is a well-known behavior identical to the heat flux characteristics occurring in rocket motors. The heat flux pattern results because the mass flow per unit flow area rapidly increases to a maximum in the nozzle throat and convective heat flux varies directly with this parameter. The results shown in Figure 2 clearly identify the three levels of combustion temperature achieved with the three N/O ratios (0.40, 0.70 and 1.00) at a constant stoichiometry.

Corresponding with the heat flux levels previously presented are plots of the cumulative heat losses along the combustor system shown in Figures 5 through 8. The overall heat loss for the cases represented in Figure 5 is about 9-10% of the total thermal input to the system. The maximum heat loss measured during the tests reported here was 2.64 MW (case LMF1C-3(6) in Figures 3 and 7) which corresponds to a 11.3% of the thermal input. The total heat loss in percent of the total thermal input are plotted as a function of calculated adiabatic flame temperature in Figure 9. In general, the heat loss from the system is typically between 8 and 12% of the thermal input to the combustor system for the variety of conditions specified in Table I.

COMBUSTOR PRESSURE MEASUREMENTS

A relative indication of combustion efficiency can be found by comparing measured chamber pressure with theoretical chamber pressure, which is predicted based on equilibrium chemistry (NASA SP-273 code) for each set of flow conditions. The pressure measurements in the coal-fired combustor are affected by slag accumulation in the static pressure ports which can produce erroneous pressure readings. Thus reliable measurements are not available for all test points. The measurements in the combustor are made through three static pressure ports in the disperser plate and one port

Figure 1. Heat Flux Distribution throughout the Combustor System for Constant Stoichiometry and N/O Ratio.

Figure 2. The Effect of N/O Ratio on the Heat Flux Distribution Throughout the Combustor System For a Stoichiometry of 0.83.

Figure 3. The Effect of N/O Ratio on the Heat Flux Distribution throughout Combustor System for a Stoichiometry of 0.92.

Figure 4. Heat Flux Distribution throughout Combustor System.
Effect of Variable Stoichiometry

■ LMF1C-3 (5)

▲ LMF1C-2 (6)

× LMF1C-2 (5)

• LMF1C-1 (5)

$\dot{m} = 5.89$ pps
(2.67 kg/s)

$\dot{m} = 6.11$ pps
(2.77 kg/s)

$\dot{m} = 6.09$ pps
(2.76 kg/s)

$\dot{m} = 6.05$ pps
(2.74 kg/s)

Figure 5. Cumulative Heat Loss from Combustor System.
Constant Stoichiometry and N/O Ratio.

▲ LMF1C-3 (5)

× LMF1C-3 (9)

● LMF1C-4 (5)

$\dot{m} = 5.89$ pps
(2.67 kg/s)

$\dot{m} = 6.98$ pps
(3.17 kg/s)

$\dot{m} = 8.03$ pps
(3.64 kg/s)

Figure 6. Cumulative Heat Loss from Combustor System.
Effect of Variable N/O Ratio.

● LMF1C-3 (6)

× LMF1C-3 (7)

$\dot{m} = 6.31$ pps
(2.86 kg/s)

$\dot{m} = 7.39$ pps
(3.35 kg/s)

Figure 7. Cumulative Heat Loss from Combustor System.
Variable N/O Ratio, Constant Stoichiometry

Figure 8. Cumulative Heat Loss from Combustor System
Effect of Variable Stoichiometry

Figure 9. LMFl Combustor Heat Loss

located at the top of the cylindrical chamber. Post test inspections indicated that the pressure ports in the disperser plate were plugged with slag while the port in the chamber was, in most cases, clear. To overcome this problem a nitrogen-purging arrangement was designed to periodically purge the pressure ports in the disperser plate. However, in time averaging the data, combustor pressure and purge pressure were averaged together creating another source of error in the data. Therefore, chamber pressure was obtained from the unpurged port. Although, for test periods spanning several hours, this port also became partially plugged with slag, the comparison of theoretical to measured pressure is good. Figure 10 shows the relationship between theoretical and measured chamber pressure versus total flow rate for conditions experienced during the LMFIC tests. Combustor pressure data is generally within 10% of the theoretical predictions.

VITIATION HEATER EVALUATION

The vitiation heater performance is not evaluated on a continuous basis since it has been checked out and is considered operational. However, in order to insure stable operation of the combustor, it is necessary to verify the temperature of the preheated oxidant, the vitiation heater static pressure and its heat rejection characteristics.

The vitiation heater is instrumented to provide a radial distribution of the oxidant temperature leaving the vitiation heater. Six thermocouples at different circumferential and radial locations were positioned axially at the heater outlet. In general, the gas temperature distribution was very uniform across the center section for all the cases analyzed as shown in Figure 11.

4.2 MHD Power Train

Major efforts of this task during the past quarter have been directed toward support of the ongoing CFFF LMF test series. This has included the evaluation of demonstrated performance for all the upstream CFFF LMF flow train components (nozzle, channel, and diffuser). A total of four tests of the LMFIC series have been completed by the end of this quarter in which an extensive amount of data were collected for varied combustor operation. A summary of the tests is presented in Table III which gives the combustor operating points of each test during which a sustained period of coal-fired operation was achieved. As is evident in Table III, each individual test consisted of combustor operation through a range of stoichiometries, N/O ratios, and total combustor throughput. These variations in combustor operation have provided a broad range of flow conditions and thermal loads over which an engineering evaluation of the gas dynamic and heat recovery of each of the upstream flow train components is being made.

A total of 21 cumulative hours of coal-fired operation has been accomplished to-date in the LMF1 tests, 10 hours in the last quarter. The experimental test trains used during these periods have included the nozzle and diagnostic channel for this entire duration. No major problems have been experienced with either of these components. The channel external sealing problem addressed in last quarterly reporting¹ appears to have been

Figure 10. Experimental and Theoretical Combustor Pressure versus Total Mass Flow Rate

Figure 11. Vitiation Heater Radial Temperature Distribution Case: LMF1C-2

TABLE III
SUMMARY OF LMF1 TEST POINTS
(DATA AVERAGED OVER 10 MINUTE PERIODS)

TEST	PT	OIL (lb/s)	COAL (lb/s)	OIL/COAL	STOICH	N/O	MASS FLOW (lb/s)
LMF1C-1	4	0.505	1.03	0.49	0.82	0.38	6.08
	5	0.504	1.03	0.49	1.08	0.29	7.02
LMF1C-2	5	0.503	1.26	0.40	0.78	0.41	6.09
	6	0.505	1.24	0.41	0.80	0.40	6.13
LMF1C-3	5	0.503	1.15	0.44	0.82	0.40	6.01
	6	0.505	1.10	0.46	0.94	0.41	6.33
	7	0.506	1.15	0.44	0.92	0.73	7.38
	8	0.502	1.05	0.48	0.89	1.0	7.81
	9	0.505	1.00	0.51	0.84	0.70	7.00
	10	0.505	1.09	0.46	0.79	0.39	5.71
LMF1C-4	5	0.502	1.16	0.43	0.83	1.02	8.00
	6	0.502	1.15	0.44	0.91	1.00	8.50
	7	0.609	1.36	0.45	0.82	0.74	8.46
	8	0.606	1.34	0.45	0.94	0.71	9.12

solved. The channel was visually monitored using closed circuit TV during all tests and visual inspections of channel sealer were made following each test. No evidence of any significant plasma leakage was apparent.

The original LMF diffuser which was lofted to be compatible with the LMF MHD generator test train was replaced this quarter. This modification to the upstream test train took place prior to test LMF1C-3 and the "replacement" diffuser will remain in the test train for the culmination of the LMF1 test series. Modification to the diffuser was addressed in the last quarterly reporting.¹ It consisted of replacing the downstream lofted sections of the original LMF1 diffuser with a single, large constant area section to form an abrupt expansion chamber into which the plasma from the channel exhausted. Details on the replacement diffuser design and aerodynamic data collected on its performance are discussed and presented in later sections.

Measurements of the diagnostic channel frame-to-frame electrical resistance were made this quarter to provide the background information necessary for the design of hardware and instrumentation to be used in plasma conductivity measurements. The resistance measurements revealed that a significant degradation of the electrical isolation of the window-frame channel had occurred since installation. In general, approximately the downstream two-thirds of the channel was essentially shorted. It was also determined that this shorting was not external to the channel, i.e. through the facility cooling system. The exact cause of the shorting has not been identified at this time.

Measurements of plasma electrical conductivity were pursued during tests LMF1C-2 and LMF1C-4. Results of the measurements are at the present time inconclusive. Details on the nature of the conductivity measurements and the data collected are examined in following discussions.

LMF1 DATA AND PERFORMANCE ANALYSIS

Data gathered during the LMF1 test series include pressures and heat flux levels throughout all upstream flow train components. The data are reviewed on-line during each test as a means of identifying proper, safe operation. In addition, each test data set is extensively scrutinized for any anomalies in post-test analyses. The analyses encompass comparisons between the measured and predicted/design performance levels. All LMF1 upstream test data reviewed to date have demonstrated operation well within the design specifications for each of the individual components.

COMBUSTOR OPERATION

Combustor operation is discussed in this subtask only as it related to entrance conditions to the nozzle/channel.

An illustration of the flow train operating points achieved in this quarter is given in Figure 12. This figure identifies as discrete data points the stoichiometry and N/O ratio for each of the test points of Table III.

(REACTANT FLOW RATE ~ 4.0 to 9.0 LB/S)

Figure 12. Summary of LMF1C Operating Conditions

As mentioned in subtask 4.1, the operating points selected for the LMF1C testing were selected to provide a significant variation in the combustion effluents, in particular, NO_x and SO_x levels. A broad variation in the combustion flame temperatures results with operation throughout this range and consequently a significant variation of the heat load on each of the individual upstream components results. To illustrate this range, a map of the adiabatic flame temperature as computed using the NASA SP-273 chemical equilibrium code is given in Figure 13. This map shows the influence of the primary combustion variables; stoichiometry, total mass flow rate, and N/O ratio, on adiabatic flame temperature for fuel compositions as dictated for LMF1 "base" conditions. This temperature can be viewed as the combustor stagnation state theoretically possible for the LMF1 combustor with complete combustion of fuels to dissociated equilibrium.

The large variation in temperature with oxygen enrichment are demonstrated in Figure 13 by the four N/O surfaces plotted. Slight variations (or curvature) of each N/O surface occurs through the range of stoichiometries considered. A general increase in the temperature follows increased mass flow rate which is in keeping with supersonic operation of the LMF1 diagnostic channel and the fact that the flow is choked at the nozzle throat. This relatively broad range in the theoretical, adiabatic flame temperature (as much as 250K) reflects the significant variation of the heat load to which the various upstream components will be subjected. This is particularly true for those components, such as the combustor and diffuser, which are dominated by radiative heat transfer.

UPSTREAM HEAT RECOVERY

Global heat loss measurements for upstream flow components obtained during LMF1C tests are presented in Figures 14 through 16. The data were extracted from the overall data set of the LMF1C tests. Each data point represents averaged data over a two-minute period and the entire set of points encompass a ten-minute period at which stable flow conditions were established as indicated in Table III.

Figure 14 shows the combined combustor and nozzle total heat loss for the variety of combustor operating conditions plotted against both the total mass flow rate and adiabatic flame temperature. This figure is similar to that presented in the last quarterly reporting¹ and extends those results to include the LMF1C series tests of this quarter. In general, the overall combustor and nozzle heat loss data of Figure 14 conforms to theoretical trends, i.e. higher net loss is associated with increased mass flow rate and flame temperature. A comprehensive review of this particular heat loss is pursued on an on-going basis as more LMF1 test data become available. It is particularly important in MHD applications of the combustor and nozzle since this loss directly impacts the channel entrance plasma temperature thus the plasma electrical conductivity. Over the range of test conditions to-date, this loss has remained within 2.0 to 3.0 Mw which represents between 8.0 to 11.0% of the available thermal input. Theoretical combustion calculations for base operation of the LMF1 flow train, i.e. mass flow rate of 3.63 kg/s, stoichiometry of 0.83, and N/O of 0.4, subject to these levels of heat loss yield nozzle exit/channel entrance conductivities in the 8.0 to 10.0 mhos/m range.

Figure 13. Adiabatic Flame Temperature Variation for LMFl Flow Train

ACCUMULATED LMF1 TEST RESULTS
VARIABLE STOICHIOMETRY
VARIABLE N/O RATIO

Figure 14. Combustor and Nozzle Heat Loss

ACCUMULATED LMF1 TEST RESULTS
 VARIABLE STOICHIOMETRY
 VARIABLE N/O RATIO

Figure 15. Channel Heat Loss

ACCUMULATED LMF1 TEST RESULTS
 VARIABLE STOICHIOMETRY
 VARIABLE N/O RATIO

Figure 16. Diffuser Heat Loss

Figure 15 presents global heat loss measurements for the diagnostic channel during LMF1C tests. The data were obtained from cooling water heat balances over the five generator modules. Channel heat loss has generally been of the same order of magnitude as combustor and nozzle heat loss combined. A further evaluation on the nature of this loss is addressed in following discussions on theoretical modeling of the channel gas dynamics. This region of the upstream flow train is dominated by convective heat transfer since flow velocities are high and the gas temperature level is significantly reduced from its combustor stagnation level because of the expansion to supersonic flow conditions.

Figure 16 presents the measured total heat loss to all of the diffuser sections. Data from LMF1C-2 were for the original diffuser design, and those of the later tests were for the replacement diffuser. The higher levels of global heat loss with the new diffuser were expected from design calculations. This higher heat loss with the replacement diffuser results primarily from the fact that this design forces the plasma to shock from supersonic to subsonic flow regime farther upstream than the original diffuser design. This situation leads to a larger volume of the subsonic, high temperature flow downstream of the shock and a greater surface area of the diffuser exposed to radiative heat transfer that ensues. At the same flow conditions, the replacement diffuser is subject to as much as a 30% greater heat loss than was the original LMF1 diffuser design. Prior to installation of the replacement diffuser the large expansion section was lined with refractory in an attempt to minimize this effect.

As a final look at the demonstrated heat recovery of the LMF1C flow trains, the heat loss of the nozzle, channel and diffuser is added to the combustor heat loss data from subtask 4.1 preceding and is presented for review in Figure 17. This level was seen to generally fall between 6.0 to 8.0 Mw throughout the LMF1C testing. The range of thermal inputs for these tests based on the available energy of the fuels was 23.0 to 28.0 MW. At base flow conditions, overall upstream heat recovery is in the range of 25 to 30% of the combustor thermal input.

The total upstream heat loss levels exhibited in Figure 17 are strongly dependent upon both the total mass flow rate and the combustor reactant mixtures. These parameters impact the degree of heat transfer in the upstream flow train components. Mass flow rate (or alternatively Reynolds Number) has a strong influence on the convective transfer within the system and also influences the combustor flame temperature level as demonstrated in Figure 13. Combustion reactant mixture, in particular the nitrogen-to-oxygen ratio, resulted in flame temperature differences of as much as several hundred degrees Kelvin which significantly influence the radiative heat transfer in the combustor, channel and diffuser.

UPSTREAM FLOW TRAIN GAS DYNAMIC DATA

Experimental heat flux and static pressure data from all of the test points of LMF1C that were obtained this quarter are presented in Appendix A. The data are typical in conformation to previous LMF1 flow train results obtained around base operating conditions.

ACCUMULATED LMF1 TEST RESULTS
VARIABLE STOICHIOMETRY
VARIABLE N/O RATIO

Figure 17. Total Upstream Heat Recovery
(Combustor through Diffuser)

Heat flux distributions presented in Appendix A result from two-minute averages of the data obtained at stable operating conditions for each test point of LMF1C (a minimum of 116 discrete data points). Channel heat flux is computed from local cooling water heat balances on selected frames of the diagnostic channel and fairly well represent the local heat load. The diffuser heat flux data are extracted from cooling water heat balances on each of the large diffuser sections. Location of the data points on the distribution plots in Appendix A was selected to be the center location of each section.

Pressure distributions were obtained from wall static pressure measurements located throughout the channel and diffuser. The data were selected from the data set of each test point by inspection of the instantaneous data to verify that accurate pressure measurements were obtained that were free from slag blockage and not influenced by the dry nitrogen purge system.

ANALYTICAL MODELING AND DATA EVALUATION

Various modeling techniques that have been developed in the past at UTSI have been applied to the LMF1 flow train to qualify the performance of the flow train components, including both one-dimensional and multi-dimensional steady state, numerical fluid dynamic computer programs. Results of previous studies on the CFFF LMF flow train using these techniques have been previously reported^{2,3}.

An extensive effort is in progress to extend the computations to include three-dimensional fluid and electrodynamic analyses of the CFFF LMF DCW generator. This work is part of a general effort to provide background information necessary to formulate the test plan for MHD generator channel experiments which will be pursued in the future LMF2 test series. This study was discussed in the last quarterly report computations were presented on aerodynamic and nominal B-field operation of the LMF DCW generator. During the present quarter this analyses has been extended to examine operation at higher B-field levels to verify the method for higher interaction operation. Results of this overall effort are presently being prepared for publication and will be reported in a future topical report⁴.

LMF1 MODELING

To permit analysis of the gas dynamic behavior of the upstream LMF1 test train and to quantify the data that are being accumulated from the LMF1C test series a one-dimensional, steady flow model has been developed which is tailored to the LMF1 test train. The model is part of an overall modeling effort at UTSI to evaluate the performance of the entire LMF1 flow train from combustor through the exhaust stack. The model solution technique employs a standard Runge-Kutta numerical scheme approach for solution of the one-dimensional form of the governing flow equations^{2,3}. The model provides a direct, first order approach for analyzing LMF1 test data and projecting performance levels of the upstream components at untested conditions. Computations performed with an early version of the model were presented in the last quarterly report and compared with experimental data collected during the first test of the LMF1C test series.

The general form of the governing flow equations solved in the LMF1 upstream model are summarized in the following;

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

$$\text{Momentum:} \quad \rho u (du/dx) + dP/dx = -4 \tau_w/D_h \quad (2)$$

$$\text{Energy:} \quad \rho u (dh/dx) + \rho u^2 (du/dx) = 4 \dot{q}/D_h \quad (3)$$

$$\text{State:} \quad P = P(\rho, T) \quad (4)$$

To close this set of equations, the relationship between the thermodynamic properties (equation 4) is obtained through curve fits to external solutions of the combustion plasma using standard chemical equilibrium calculations for the particular combustion reaction. The source terms of the governing equations are computed using empirical correlations for turbulent boundary layer over rough surfaces⁵, including the evaluation of wall shear and the convective heat transfer. Evaluation of the local heat flux term of the energy equation includes both the contribution from convection along the wall and an approximation of the net radiative heat exchange between the gas stream and the channel surfaces.

Radiative flux is computed locally under the assumption that the chief radiative exchange at any axial location is primarily the result of temperatures in the immediate surroundings. This formulation is tantamount to considering the entire enclosure as a single isothermal zone in radiative equilibrium. The net radiative flux to an incremental surface area is approximated by the closed form expression;

$$\dot{q}_{\text{rad}} = (\epsilon_w / (1 - (1 - \epsilon_w)(1 - \alpha_g))) \sigma (\epsilon_g T_g^4 - \alpha_w T_w^4) \quad (5)$$

where ϵ_g and α_g are the emissivity and absorptivity of the gas based upon its local mean temperature and equilibrium chemistry, ϵ_w and α_w are the emissivity and absorptivity of the wall surface.

The radiative transport properties of the combustion gas are computed considering CO₂, CO, and H₂O as the principal contributing gas species along with a nominal estimate of the emission from solid particles. The general form of the total combustion gas emissivity is evaluated using the following expression which employs a gray gas approximation:

$$\epsilon_t = 1 - (1 - \epsilon_{\text{CO}_2}) (1 - \epsilon_{\text{CO}}) (1 - \epsilon_{\text{H}_2\text{O}}) (1 - \epsilon_p) \quad (6)$$

or alternatively;

$$\epsilon_t = \epsilon_g + \epsilon_p - \epsilon_g \epsilon_p \quad (6a)$$

The individual emissivities of the gas constituents (ϵ_{CO_2} , ϵ_{CO} , $\epsilon_{\text{H}_2\text{O}}$) and the gas species as a whole (ϵ_g) were computed using curve fits of emissivity versus temperature data obtained from NASA Report SP-3080 together with Hottel's charts with an applied pressure-broadening correction^{6,7}.

The mean beam length used in interpreting the parameters from curve fits is evaluated at each axial location based upon the physical geometry of the system. A general formulation for the particle emission (ϵ_p) follows that proposed in reference 8. This technique employs the small particle approximation to Mie theory which yields the following general form for the emissivity for slag particles:

$$\epsilon_p = (1 - e^{-\kappa L}) \quad (7)$$

where L is the physical path length and κ is the effective soot parameter and is defined as,

$$\kappa = +(3.6 C_s T_g / C_2) \quad (8)$$

where C_2 is Planck's second constant (1.4387 cm-K), T_g is the mean gas temperature, and C_s is given by:

$$C_s = 36 \pi f_v \left((nk)/(n^2 - (nk)^2 + 2)^2 + (2nk)^2 \right) \quad (9)$$

In the above functional expression, f_v represents the volume fraction of slag particles and the parameters n and k are the real and imaginary parts of the complex index of refraction which were derived as independent of wavelength with scattering ignored. In the present modeling, the values of n equal to 1.5 and k equal to 2.6×10^3 are based on material properties of slag.⁹

The radiation modeling is used in the gas dynamic calculations throughout the channel and diffuser, and provides a simple, reasonable means of approximating the magnitude of the net radiation load on these components. Treating the system as a single isothermal zone will tend to overpredict the radiation in the upstream part of the channel and underpredict it in the downstream portion. However, the net radiation load on the channel is small in comparison to the convection and this should not result in any significant error. In the diffuser, the heat transfer to the surroundings is dominated by radiation since gas velocities in this component are relatively low. The single zone approach applied to the diffuser is believed to be quite reasonable since only a slight axial gas temperature gradient is present and the system boundaries are at near constant temperature, as verified by a comparison of the predicted diffuser heat loss to experimental measurements.

Local heat transfer along the flow train either by convection or radiative exchange is dependent upon the local wall temperature which in most cases must be specified either based upon experimental evidence or simply through an engineering approximation. In the present model, a regional value of wall conductance was chosen as a parameter governing the local heat transfer, so that the net exchange of heat from the plasma through the wall to the cooling passage is considered. To illustrate this technique, consider the sketch shown in Figure 18 which shows a simple control volume that nearly typifies the physical configuration for cooling the upstream components. An overall heat balance extending from the plasma-wall interface through the wall to the cooling passage can be written as:

Figure 18. Surface Control Volume for Heat Transfer Modeling

$$q = q_{\text{rad}} + q_{\text{conv}} = q_{\text{convH}_2\text{O}} = U(T_w - T_{\text{H}_2\text{O}}) \quad (10)$$

where U represents an overall conductance of the cooled wall structure. Generally speaking, U can be a function of many variables within the confines of the control volume. These include material properties of the wall, water side convection, the geometric configuration of the wall and its cooling passages, and the slag layer, its properties and thickness along the wall-plasma interface. An exact mathematical treatment of this parameter would prove quite rigorous and is not considered worth pursuing since even this approach requires guess estimates of unknown geometric and physical parameters. Rather it proves more fruitful to treat the wall conductance of equation 10 as a local parameter within the overall heat transfer problem. In this fashion, a plasma-wall interface temperature can be computed which is a reflection of the systems ability to transfer the surface heat load to the cooling water.

Considering the plane wall situation as depicted in Figure 18, equation 10 can be recast into the following form,

$$f(T_w) = c(\epsilon T_g^4 - \alpha T_w^4) + h_{\text{conv}}(T_g - T_w) - U(T_w - T_{\text{H}_2\text{O}}) \quad (11)$$

where $c = \sigma \epsilon_w / (1 - (1 - \epsilon_w)(1 - \alpha_g))$ (from equation 5), $T_{\text{H}_2\text{O}}$ represents the mean (bulk) temperature of the cooling water within the surface cooling passages, h_{conv} is the plasma surface convective heat transfer coefficient, and all other terms are as defined previously. Treating the wall conductance as a parameter in the above expression, the following equation results.

$$f'(T_w) = -4c\alpha T_w^3 - h_{\text{conv}} - U \quad (12)$$

The set of equations must satisfy the wall temperature boundary condition.

$$f(T_w) = 0 \quad (13)$$

Computation of the local wall temperature which satisfies this constraint can be accomplished at each numerical step through a standard iterative procedure for finding the roots of the polynomial in T_w as defined by equation 11. In the present modeling this is accomplished using equations 11 and 12 combined through a Newton-Raphson iterative procedure.

In adapting this approach to analysis of the LMF1 testing, the flow train upstream can be divided into distinct heat conductance regions which are distinguishable according to wall materials and the configuration of the water cooling passages. The regions are the channel, the smooth-wall diffuser upstream sections, and the refractory lined downstream diffuser section. Structural materials for the components are copper, austenitic stainless steel, and carbon steel, respectively. Cooling of each component is accomplished using gun drilled water passages near the plasma interface surface or through flow of water in an internal annulus. The design cooling water flow rates were specified to maintain a sufficient degree of water side convective heat transfer to maintain the plasma side surface temperatures at safe operating levels. Flow rates to each component were constrained to a maximum 10% variation between individual tests because of safety considerations.

Based upon the experimental observations, fixed nominal conductance can be applied to each of the upstream components to analyze the local heat transfer at varying combustor operating conditions. This approach was used in the present study and incorporated into the modeling. Computed values of wall temperature, local heat flux, and overall heat loss to each of the components are compared to experimental data to verify wall conductance values. Once an acceptable value is determined for a given data set, it can be treated as constant to extend the computations to other test conditions.

MODELING OF THE DOWNSTREAM SHOCK

Examination of the pressure data collected during the LMFl test series, reveals that the flow field shock system within the channel exit/diffuser entrance region exhibits characteristics typical of interaction between shocks and thick, turbulent boundary layers. The most significant characteristic is the tendency of the shock system to extend over a significant length of the duct. Additionally, boundary layer separation just downstream of the shock system termination point can be distinguished in most data sets, as indicated by the marked flattening of the pressure distribution. The features observed are typical of a structured shock system within a confined duct.

Extensive research has been conducted in the past to define the behavior of shocks confined within ducts. Unfortunately most flows investigated have been well behaved, clean gases rather than the multiphase plasmas typical of coal fired MHD applications. Review of the confined shock systems research reveals that in most systems shock system structure is a result of boundary layer separation induced by the extreme shock wave pressure rise. The degree of shock system extension in the duct depends on the pressure rise and the boundary layer thickness at the point where the shock ensues. Typical flow patterns within a confined, constant area duct are presented in Figure 19. The behavior of the shock structure as a function of the boundary layer thickness is clearly illustrated. The shock structures vary in character from a near normal shock wave in the presence of a very thin boundary layer to a system of intersecting oblique shocks in which the normal portion of the wave has vanished.

The LMFl data have demonstrated shock patterns of the type depicted in Figure 19b, and in the limiting case that of Figure 19c. Such patterns result from a more gradual rise in static pressure through the shock region rather than an abrupt pressure jump that would accompany a normal type shock. This behavior is consistent with past two-dimensional flow field calculations performed for the LMFl diagnostic channel which predicted the development of a relatively thick boundary layer at the channel exit.^{2,3,4} Boundary layer blockage of 20 to 30% of the available flow area at the channel exit was predicted, depending on flow conditions and wall roughness parameter. Approximate first-order type calculations can be done to demonstrate the probability that boundary layers of this magnitude will indeed separate in the presence of shocks. An estimate of the critical pressure rise required to separate a turbulent boundary layer can be approximated using the following expression¹¹;

$$\Delta P_{crit} / P = 2.25 \gamma M^2 / Re \cdot 2 \quad (14)$$

Figure 19. Typical Boundary layer Shock Wave Interaction

Considering the pressure rise associated one-dimensional wave theory formation of a normal shock

$$\Delta P_{\text{normal}} / P = \left[2 \gamma / (\gamma + 1) \right] (M^2 - 1) \quad (15)$$

subject to nominal flow conditions (Re between 10^5 and 10^7), tripping of the wall boundary layer in the LMFI channel can be anticipated when the entrance Mach number exceeds 1.3. The local Mach number in supersonic operation is exceeded throughout both the channel and the diffuser. Formation of a shock within these regions will lead to separation of the wall boundary layers and create a shock zone that can extend several duct diameters depending upon local boundary layer thickness. Such behavior has been observed during operation of the LMFI flow train.

To provide a qualitative model of the shock zone, an approach has been adapted which draws upon shock-boundary layer interactions research and observation of data from the LMFI testing. Previous efforts to form approximate one-dimensional models of shocks within MHD type systems have for the most part considered the shock to be normal in form but of a reduced strength¹⁰. This approach permits a location of the shock at an axial location consistent with experimental observations and still maintains the net overall discontinuous effect on gas dynamic properties which allows matching of the system back pressure. Alternatively, the shock can be computed as a true normal, one-dimensional wave that positions itself within the flow train at a point where back pressure conditions are matched, resulting in a shock location downstream of the experimental location. Both approaches require numerical iteration to achieve reasonable

agreement to the back pressure imposed on the system. The first approach iterates on the net strength of the fixed position shock using experimental data. The second approach calculates shock location assuming a fixed shock strength equal to true normal shock strength.

Both approaches are acceptable for one-dimensional calculations, since realistic representation of the shock-boundary layer separation zone requires more sophisticated modeling. The applicability of either approach depends on the required accuracy of shock system position and size within the flow train. In MHD applications, these characteristics can become important at off-design conditions where the shock location is within the generator channel, leading to a degradation in generator performance and potential electrical faults occurring because of the extreme plasma property gradients.

In the present study, the method used to model the shock discontinuity draws upon a qualitative description of the shock zone and treats the entire region. A review of the governing equations for normal shocks and noting that the friction forces in a separated region of the shock zone are negligible, it can be formulated that the overall pressure rise through the shock zone should be substantially the same for an idealized normal discontinuity¹¹. This ideal is used herein in the determination of the local shock strength within the structured shock zone. The overall pressure rise through the zone does not conform completely to the above in that radiative heat transfer, which is not necessarily negligible, is accounted for. In the present study, the shock zone is treated as a series of pseudo-shocks (or compression waves) of varying strength. The term pseudo-shock indicates that the flow may be supersonic behind the shock.

The location of the shock zone, a region from initiation of the shock system until subsonic flow is achieved, is accomplished through an iterative procedure. In this procedure, a candidate location is chosen and the shock zone is computed. Downstream of the shock zone the flow field is computed according to the nominal procedure as governed by equations 1 through 4, disregarding viscous effects. At the end of the flow train, the static pressure is compared to the system back pressure (nominally 0.97 to 1.0 atms is used in analyzing the LMF1 flow train) to see if a predefined matching tolerance is met. If the calculated system back pressure is within tolerance, a solution has been achieved; if not, the shock is relocated and the computational procedure is repeated. This iterative approach is similar to the type mentioned above for locating shocks within a confined duct.

Details on the computational procedure used in approximate modeling of the shock zone can best be understood by referring to Figure 20 which depicts a comparison between a physical view of the zone to the mathematical approximation employed. In computing this region a nominal step size, Δx , is chosen. Based upon the entrance Mach number, the pressure loss across a normal shock is computed according to equation 15, and an overall length of the shock zone is estimated using the following polynomial,

$$L/\delta = -20.0 + 25.4 M - 5.4 M^2 \quad (16)$$

which was obtained through data curve fits for shock zone length as a function of Mach number in reference 11. In the above expression, δ is the mean boundary layer thickness computed from the flow area blockage assuming equivalent thickness on all walls. The blockage is treated as a parameter and is an input based upon past two-dimensional calculations.^{2,3,4} At the point at which the first shock forms in the zone the boundary layer is assumed to trip and grow linearly through the shock length.

Figure 20. Representation of 1-D Modeling of the Shock Zone

The pressure distribution through the zone is computed assuming a linear profile. From this profile the pressure at the next step is obtained by the following;

$$P_{x+\Delta x} = (\Delta P/L) (\Delta x) + P_x \quad (17)$$

This pressure is used in computing the strength of the first shock (or compression wave) in the zone. This reduced strength shock is then used in computing the Mach number on the downstream side of the wave as;

$$M_x' = ((P_x / P_{x+\Delta x}) (1 + \gamma M^2) / \gamma - (1/\gamma))^{.5} \quad (18)$$

Given the downstream Mach number (M_x') and pressure, the thermodynamic state of the gas is thus defined. In Figure 20 (as point 0 to 1 typically) the flow field in the region between shocks is computed as a Rayleigh type flow. Once the flow field computation is completed between waves, a new upstream condition to the next compression is defined and the process is repeated, i.e. compute a new pressure gradient, shock strength,

and downstream thermodynamic state. The entire procedure continues until the Mach number following a wave becomes close to 1.0 (nominally 1.01 is used as a stopping criteria) at which time the shock is assumed to be normal which decelerates the flow to subsonic. From that point the flow is computed by the standard method, including both the wall shear and heat transfer terms.

The fundamental length of the shock zone computed is determined from the input blockage. Note that equation 16 implies that at a blockage of zero (or zero boundary layer thickness) a normal shock exists. Computations performed have shown that nominal blockages of between 20 to 30% of the flow area yield overall lengths of the shock zone that reflect those seen in the LMFI data. The levels agree with the exit blockage computed for the LMFI channel in previous two and three-dimensional flow field computations. The method described above was derived for the simple case of a constant area duct. In the LMFI diffuser the shock structure occurs within the diverging sections of the diffuser at flow rates greater than about 2.27 kg/s (5.0 lb/s). To account for area variation in the modeling, the blockage is adjusted within each region between compression waves by an amount equivalent to the loft area change. This can be expressed as,

$$B_{x+\Delta x} = B_x + (B_x/L_x) \Delta x - (\Delta A) \quad (19)$$

where B is the flow area blockage defined according to the following;

$$B = (2\delta(h + w) - 4\delta^2)/wh \quad (20)$$

This adjustment in the case of a diverging duct leads to stronger compression waves and a shorter overall shock zone length which is consistent with the physical situation.

The intent of going to such an extreme effort in modeling the shock zone as has been discussed above may not be apparent to the reader since this area of the flow stream can be treated successfully in other simpler manners, such as those mentioned previously. However, in view of the myriad of data being generated in the CFFF LMFI tests under a broad range of varying supersonic flow conditions, a broad data base is being accumulated on the aerodynamic behavior of coal combustion driven plasmas. These data need to be characterized against existing aerodynamic data to identify anomalies that occur which may have implications in the design of future MHD channels and diffusers. In terms of the shock zone, this region is generally considered a gray area from a quantitative standpoint without the availability of sophisticated gas dynamic models. As the first order approach used herein is evaluated against data, an aid to design of MHD diffusers may be drawn. In particular, it will be applicable to the physical lofting and sizing of supersonic diffusers to contain the shock zone efficiently.

COMPARISON OF ANALYTICAL AND EXPERIMENTAL RESULTS

A comparison of the theoretical predictions from the model described above to upstream gas dynamic data collected from tests of the LMFI series is given in Figures 21 and 22. This comparison is drawn against static

Figure 21. LMF1 Static Pressure Distributions
(Distance Measured from Channel Entrance)

HEAT FLUX

W PER CEN

Figure 22. LMF1 Heat Flux Distributions
 (Distance Measured from Channel Entrance)

wall pressure data and heat flux data of the type presented in Appendix A for tests LMF1C-3, step 5 and LMF1C-4, step 6. The particular test points were chosen for comparison to predictions since they are near base combustion reactant mixtures and base total flow rate, respectively.

As seen through comparison of the data to theory, the one-dimensional model used to analyze the LMF1 upstream flow train provides a good representation of the gas dynamic behavior throughout both the channel and diffuser. In computing each of the two cases shown the same set of empirical parameters were applied to provide a common basis between the two distinct flow conditions. These parameters included wall roughness (0.5 mm roughness height), wall thermal conductance values in the channel and both diffuser sections, and channel exit (diffuser entrance) flow area blockage (30%).

An evaluation of the aerodynamic behavior of the LMF1 upstream flow train can be drawn through a comparison of the computed pressure distributions with the experimental data shown in Figure 21. The flow accelerates throughout the major portion of the channel which is indicative of supersonic flow in a diverging duct. Inflections in the pressure distribution at the approximate locations of 0.7 meters and 3.0 meters are in fact inflections in the channel lofting that are physically present. A slight deceleration of the plasma is evident in the computations just downstream of the 3.0 meter point at which point the channel loft changes from diverging to constant area. This constant area section was originally designed in the channel to provide a mating to the first constant area section of the original LMF design diffuser. It is within this region that the downstream shock zone should locate under powered operation of the LMF DCW generator. However, for operation under unpowered channel conditions as in LMF1 tests the shock zone initiates inside the .0349 radian (2°) diverging section of the first diffuser section. This point moves downstream with increasing mass flow rate or rather combustor total pressure. For the low mass flow case the shock initiates just inside the diffuser entrance, for the high mass flow case initiation occurs approximately 0.5 meters further downstream. The length of the shock zone computed for both cases shown predicts the actual length well as inferred from the pressure data. A marked separation is apparent in the pressure data of the low mass flow case just downstream of the point where the shock zone terminates. This character fully illustrates the tripping of the boundary layers caused by the presence of the shock. In the large, constant area section of the diffuser a general diffusion of the flow field is indicated by the gradual rise in static pressure through its length. As the flow stream enters the large downstream diffuser section it must undergo a very rapid expansion which leads to a separation of the wall boundary layer and creation of recirculation regions at the entrance. The gradual diffusion of the flow field downstream of the entrance and throughout the remaining portion of the diffuser is indicative of a tendency of the flow stream to feel the wall and begin reattachment. In both cases shown in Figures 21-22 diffusion of the flow is shown to be complete, at least from a one-dimensional standpoint. Static pressure data at the exit is consistent with the downstream back pressure in the radiant furnace.

A comparison of the predicted and experimental heat flux data presented in Figure 22 again demonstrates the capabilities of the model to characterize the actual LMFI upstream flow train. This agreement is considered exceptionally good for the low mass flow case. In the high mass flow case a marked overprediction of the local heat flux level in the entrance region of the channel is apparent. This is believed to be a result of the tendency for radiation modeling to overpredict the radiation loading in the front end of the channel. In both sets of experimental data a "bump" is seen to occur in the measured heat flux at around 1.0 meters from the entrance. This bump has been seen in most all of the data sets of LMFI testing and may be the result of some small separation of the boundary layer within the channel which is set off by the slight inflection point that occurs in the loft at 0.78 meters. This is typical behavior of heat transfer around a separation point in that a drop occurs behind the separation point and a sharp rise occurs at the point where the boundary layer reattaches to the wall. Heat flux levels predicted in the diffuser sections agree quite reasonably with those computed from global heat loss measurements. The experimental data point in the first diffuser section (3.75 meters) was plotted at the mid-point of this section which lacks sufficient resolution for a meaningful comparison with the detailed calculation. The peak heat flux to it will occur after the shock due to higher radiation loads. Proper plotting of this point would probably be biased toward the downstream end rather than at its midpoint.

Other pertinent gas dynamic properties of the two LMFI tests investigated above are shown in Figure 23. Here only predicted lines are given since no data on these particular parameters are available. This figure shows the predicted behavior of the gas temperature, wall temperature, velocity, and total pressure through the upstream components. These properties appear to conform to the nature of the flow field in these components as has been previously discussed.

Also shown for comparison in Figure 24 is the cumulative heat loss as predicted by the modeling for both LMFI tests being considered. These predicted lines were drawn from an integration of the heat flux calculations along the channel and diffuser lengths. As can be seen in this figure, the predicted heat loss is within a very small percentage of the global heat loss measured from an overall cooling water heat balance of these components. Obtaining a relatively good agreement with this particular measurement is of prime importance in the upstream modeling effort. Good agreement here indicates that the modeling is adequate in predicting the diffuser exit temperature/radiant furnace entrance temperature in keeping with an overall thermodynamic balance across the upstream flow train. Exit conditions from the present model are used as inputs to further computations of the gas dynamics and heat transfer in the downstream components.

COMPARISON OF LMFI DIFFUSER DESIGNS

Figure 25 presents pressure distributions taken from LMFI test data for the upstream flow train. The two data sets were chosen for presentation since they are at almost identical flow conditions and illustrate well the characteristics of the two distinct diffusers used in LMFI

Figure 23. LMF1 Gas Dynamic Distributions. Distances Measured from Channel Entrance.

Figure 24. Cumulative Heat Loss from Channel and Diffuser versus Theoretical Predictions
(Distance Measured from Channel Entrance)

Figure 25. Comparison of LMF1 Diffuser Designs Demonstrated Pressure Distributions
 (Distance Measured from Channel Entrance)

testing. The LMF1A data were taken on the original diffuser and the LMF1C data were taken with the replacement diffuser. The distinctly different loft of the two designs can also be seen in this figure.

As previously discussed incorporation of the replacement diffuser into the LMF1 flow train was necessary due to the nature of the damage on the slag screen. Post test examinations of this component revealed that the original diffuser design was creating a high velocity gas core because of separation of the boundary layer within the diffuser. The core impacted the slag screen causing severe erosion of its surfaces. This separation was being set off initially due to shock-boundary layer interaction. In order to minimize this damage, the replacement diffuser which has a rapid expansion section was placed in the flow train to force the separation point farther upstream in the diffuser and thus allow a longer diffuser length in which the boundary layers could reattach. The data of Figure 25 well illustrates that this effect was achieved. At the same flow conditions, the diffuser shock zone is forced farther upstream in the replacement diffuser than in the original design. A diffusion of the subsonic flow field downstream of the shock is suggested in both sets of data with the replacement diffuser at a pressure level near atmospheric slightly farther upstream.

A review of the LMF1 data sets to date indicate the shock location for both diffuser designs as summarized in Figure 26. This figure shows the experimental location of shock initiation as a function of total mass flow rate. Generally the replacement diffuser forces the shock upstream when compared to the original design as expected. The other characteristic revealed is the smaller variation in the position of the downstream shock with the replacement diffuser over the mass flow rate range tested. This character is consistent with the fact that the replacement diffuser incorporates a 0.0349 radian (2°) diverging section mated directly to the channel exit.

SUMMARY OF PLASMA CONDUCTIVITY TESTS

Measurement of the plasma conductivity was attempted on Tests 2 and 3 of the C test series. The measurements were unsuccessful in that accurate quantitative results could not be obtained because of general interframe shorting. However, the qualitative results illustrate several phases and the results are generally consistent with calculated levels of conductivity to be expected.

The schematic of the test configuration is shown in Figure 27. Basically, the measurement was performed over the region of the diagnostic channel between frames 4 and 34 which were each grounded. A voltage was applied at a mid frame position (frame 14 or 21) which resulted in parallel upstream and downstream current loops through the plasma in the channel. The total current, individual loop currents, and individual frame voltage in the region of interest were monitored as shown in the figure. The objective was to measure the voltage gradient (i.e. Hall field component) in regions removed from the power insertion frames. The axial field value could be combined with the loop current and known channel geometry to yield the plasma conductivity.

Figure 26. Approximate Shock Location for the Two Diffuser Configurations for LMFLA and LMFLC Test Series Data

(a) Power Supply Connections to Channel

(b) Typical Frame Schematic

Figure 27. Schematic of Power Supply and Instrumentation for Measurement of Plasma Conductivity During LMFIC

The setup was first tested after coal flow had been initiated during the LMF1C-2 test. The result was that several amps of current were drawn with no discernable voltage distribution. From all outward appearances, the power supply was feeding a dead short through the upstream loop comprised of frames 4 through 14. As a consequence, interframe resistors as shown in Figure 27 were added to allow static testing in the absence of a conducting plasma prior to the LMF1C-3 test.

Figure 28 presents the measured voltage distribution on the channel with 69 volts applied and no flow in the channel just prior to the LMF1C-3 test. There are six identified shorted pairs as indicated in the figure. In addition the voltage gradients appear uniform, except for the shorts, indicating that the interframe gap resistances are, as a minimum, much greater in value than the added external interframe resistors which are approximately 8 ohms each.

During the LMF1C-3 test the conductivity measuring system was on during the initial warm-up of the flow train with oil only combustion. This period lasted approximately an hour and Table IV summarizes the inter-electrode resistances derived from voltage and current measurements during this period. Since there is no seed present for this combustion, the plasma can be considered non-conductive and the variations in the interframe resistances are a result of leakage between the frames, the external shunting resistors or the parallel combination of these two. The results in Table IV are subject to instrumentation errors due primarily to reading low current levels (fractional amp) on a multi amp scale since the current required limitation to low values to prevent overloading the shunt resistors. Nevertheless, the trends indicated in the results are significant. Interframe resistances in the 7 to 9 ohm range can be interpreted as a high channel frame gap resistance shunted by the external resistor. On the other hand low resistances, less than one ohm, can be interpreted as a short in the channel interframe gap and intermediate resistances indicate that the channel gap resistance is of the same order as the external resistor. The results clearly indicate that the channel frame gap resistance deteriorates during the warm-up period for presently unknown reasons. While the deterioration is progressive it is also intermittent with respect to some of the electrode pairs, e.g. pairs 13-14 and 33-34. At the end of the preheat cycle the upstream loop measurement was essentially being sustained by only two of the ten possible interframe gap insulations.

Prior to coal flow (and seed) initiation during the LMF1C-3 Test, the interframe shunting resistors were removed and the voltage application point was changed from frame 14 to frame 21 to rebalance the two current loops with regard to the developed shorts. Dramatic changes were noted in the interframe gap resistances when the coal flow came on or went off. One such example is indicated in Table IV where coal flow was terminated between 13:29 and 13:44 and an order of magnitude change in the observed gap resistances occurs.

The presence of the shorts and the data quality preclude an accurate assessment of the plasma conductivity. However, the plasma conductivity is clearly being sensed. If it is assumed that the seeded plasma resistance

Figure 28. Pretest Measurements of Conductivity Channel Interframe Resistance with Nominal 50 Interframe Resistors Installed and Voltage Applied at Frame 14

TABLE IV

INTERFRAME RESISTANCES MEASURED DURING LMFIC-3 IN OHMS

FRAME PAIR	TIME					COAL ON 13:29	COAL OFF 13:44
	OIL ONLY WARM UP						
	9:28	9:43	9:58	10:14	10:29		
4-5	0.6	0.7	0.6	0.5	0.4	0.48	4.76
5-6	0.4	0.3	0.3	0.3	0.1	0.28	6.24
6-7	8.1	0.3	0.3	0.3	0.2	0.24	1.66
7-8	0.4	0.2	0.2	0.2	0.1	0.03	0.01
8-9	7.8	0.5	0.0	1.2	0.6	0.21	0.63
9-10	8.0	7.3	6.7	5.4	7.9	0.11	4.87
10-11*	0.6	0.5	0.6	0.6	0.0	0.06	6.40
11-12	8.4	8.4	7.8	9.2	9.3	0.84	25.3
12-13*	0.1	0.1	0.1	7.6	0.1	0.08	4.68
13-14	0.3	5.6	0.5	0.7	0.3	0.15	3.47
14-15	3.7	3.4	3.5	4.2	2.8	0.22	2.90
15-16*	0.3	0.3	0.2	0.4	0.4	0.25	3.53
16-17*	-0.1	-0.2	-0.1	9.5	-0.2	0.26	27.8
17-18	5.0	4.8	4.6	6.9	5.3	0.54	13.0
18-19	6.2	5.7	5.5	7.6	7.5	1.18	8.67
19-20	9.7	7.4	7.0	9.4	8.7	1.01	12.9
20-21	6.0	5.6	5.0	6.3	6.8	0.72	19.6
21-22	5.2	1.0	2.1	7.6	2.6	0.24	0.95
22-23	7.8	7.1	7.5	10.6	9.2	0.28	4.82
23-24*	0.0	-0.1	-0.2	6.1	-1.0	0.0	0.0
24-25	6.6	6.6	6.1	8.3	6.4	0.30	3.67
25-26	2.4	2.0	2.4	9.4	0.2	0.02	1.77
26-27	6.6	6.4	6.1	8.6	6.7	0.24	6.37
27-28	7.5	7.1	6.8	9.3	8.1	0.10	4.88
28-29	5.6	5.5	5.6	8.7	6.0	0.25	7.41
29-30	5.2	5.1	5.2	8.8	6.1	0.22	8.42
30-31*	0.0	0.0	0.1	0.2	0.1	0.03	4.38
31-32	6.8	6.4	5.9	8.9	7.0	0.49	14.5
32-33	5.5	5.1	4.8	7.8	6.2	0.30	5.14
33-34	1.1	4.0	4.2	8.0	0.1	0.47	6.60

* Known to be initially shorted

is much lower than the inherent channel frame gap resistances, then a crude estimate of the plasma bulk conductivity can be obtained. The value thus obtained is 6.5 mho/m which is consistent with calculations for this flow condition.

4.3 Downstream Components

Three separate tests, LMF1C-2, 3, and 4 were conducted in the CFFF during the fourth quarter with a total coal combustion time of 10.5 hours. Performance of the slagging furnace was acceptable, but water leaks were detected in the entrance flange to the primary furnace at a thermocouple penetration, in the interior of the Materials Test Module (MTM) at a factory weld joint, and around the plate spacers in the superheater flange door on the ash/seed hopper. All water leaks were repaired and repair of the ash/seed hopper door revealed that short sections of steel pipe instead of rods had been used as spacers between the plates of the door. Apparently the use of pipe sections instead of rods contributed to early failure of the spacer welds and leakage of the door.

Efforts were made in the fourth quarter to bring the DEMCO hydroclone into proper operation. Past efforts to use the device have been unsuccessful since most of the solid components of the cyclone water are two microns and smaller, and particles of this size are not removed by the hydroclone. A flocculant was added to the cyclone water in an attempt to increase the minimum particle size, but this had no effect on the efficiency of the hydroclone. Samples of the cyclone water will be sent to DEMCO for analysis and 5.1 cm (2 inch) cones (existing cones are 10.2 cm (4 inch)) will be tested in an effort to increase the efficiency of the unit.

This quarter several instances of molten slag blocking the slag tap were encountered. It appears that slag flowing down the furnace walls and from the slag screen enters the tapered slag tap and solidifies due to the lower temperature in this area. This solid slag gradually builds up, bridges across the 34.1 cm x 34.1 cm (13 7/16" x 13 7/16") opening in the slag tap, then continues to fill with slag as long as the furnace runs. Punching a hole in the softened slag with a steel pipe has proved somewhat successful in reinitiating the slag flow, but this solution required extra manpower and constant observation of the slag tap. The proposed solution is presented in Figure 29 and was intended to keep slag flowing through the slag tap by maintaining a flow of hot gases through the slag tap. Because of the draft created by the induced draft fan, hot gases from the primary furnace should be drawn through the slag tap into the ash/seed hopper by means of the 7.62 cm (3") diameter piping. In practice however, no effect of the piping system was observed, as the slag tap continued to plug. The next step is to replace the 7.62 cm (3") diameter piping with 15.24 cm (6") diameter piping to increase the amount of hot gas flowing through the slag tap.

The auxiliary burner used in the primary furnace to pre-heat the furnace was modified after LMF1C-2. This fuel oil burner was previously inserted into the primary furnace man door, but continued heating of the door and its hinges resulted in deterioration of the door to the extent that it would not close. The man door was then modified and secured in the closed

Figure 29. Slag Tap Diverted Hot Gas Flow

position and the lower refractory test door flange on the west wall of the primary furnace was used to insert the auxiliary burner in the furnace. In this new location, a metal shroud about 1.53 m (5 ft.) long was used between the burner and the furnace door flange. Heat radiating from this shroud during LMF1C-3 was sufficient to melt electrical insulation in wire trays about 1.53 m (5 ft.) above the burner, so a second burner shroud was installed. This shroud was about 0.61 m (2 ft.) long and incorporated an inner shroud which was cooled by air aspirated between the inner and outer shroud. This final design worked very well and will continue to be used.

Operation of the primary slag screen continues to be acceptable, as observations made after the test runs indicate that much of the slag is removed by the slag screen. A small rupture of the surface of one of the slag screen tubes was discovered after LMF1C-4, but this hole is presumed to have developed during the LMF1A test series when the water circulating pumps were shut down during the test. It was not discovered earlier because the slag screen refractory covered the area of the rupture and the water leakage (if any) was not great enough to cause further damage. The defective section of high pressure pipe was repaired in place by Facilities Lab personnel and no leaks were evident after repair.

Erratic operation of the induced draft (ID) fan was experienced this quarter. It was determined during the third quarter of FY 82 that the two stages of the ID fan required 432.5 kw (588 metric horsepower) for proper operation, but only 223 kw (303 metric horsepower) was available from the existing fan motor. In order to reduce the power requirement to about 216 kw (294 metric horsepower), the fan impellers and shaft were sent to the manufacturer and the first stage impeller was removed. This left one stage and resulted in reducing the theoretical head to 0.081 atm. When the ID fan was reassembled and operated, excessive vibration was observed and measurements indicated the shaft was bent. Peening the shaft at the apparent location of the bend resulted in a brief reduction in vibration while running, but the bend and vibration soon returned. A service representative from Zurn inspected the fan and diagnosed an imbalance. After field balancing, the fan ran well during the next test. During LMF1C-4, vibrations again developed in the fan and a crack in the metal base of the fan was observed. This crack was repaired and testing was resumed with no vibration, but a long term solution to the problem is being sought.

Additional experience was gained this quarter in the operation of the crossover duct particulate sampling equipment. Because of the short test time at a stable condition, this equipment is still being evaluated to determine the correct sampling procedure rather than to obtain a large volume of data. Work is also in progress to develop checks that will aid in early identification of questionable data and to compare data collected under the same conditions. Particulate samples were obtained from the crossover duct during LMF1C-3 and LMF1C-4 using a Flow Sensor Particle Sizing Sampler, which is a multi stage inertial impactor positioned in the process gas stream. When particulate matter passes through the stages of the impactor, the actual size collected by each stage is determined by the temperature of the combustion gas and the size, shape, and density of the dust particles. Because of the heavy dust loading in the crossover duct, sampling times are short, about one minute maximum. Results of the

measurements are given in Figure 30 and indicate the average particle size is less than 2 micrometers for the two tests. This compares well with data from the 7.5MW_t flow train in the UTSI Energy Conversion Facility.

POLLUTION CONTROL

During this quarter, gas analysis data were collected during several tests on the low mass flow train and a substantial effort was made on improving the operation and reliability of the gas conditioning equipment.

Three tests were run during this quarter, LMF1C-2, 3 and 4. A new diffuser was installed prior to LMF1C-2 and a gas sampling port was located 12.7 cm (5") from the outlet of the diffuser. This gas sampling port was used to obtain gas for NO analysis during LMF1C-2 and subsequent tests. Gas sampling ports for all tests are shown on Figure 31.

LMF1C-2 TEST

The primary objective of this test was to obtain data on NO formation and relaxation at a thermal input of 23.2 MW and N₂/O₂ ratios of 0.4 and 0.7. The vitiation heater outlet temperature was also set at two different levels, 1144K (1600°F) and 1256K (1800°F).

Limited NO data are available at this time because of the erratic coal/seed feed rates, which caused variations in stoichiometry. Since NO levels are strongly dependent on stoichiometry a more extensive analysis of the NO data are required. However, NO_x and temperature data for a N₂/O₂ ratio of 0.4 and a stoichiometry of 0.77 are shown on Figure 32. This stoichiometry is below the base condition of 0.85 recommended by UTSI, and as a result, NO_x levels are very low, only about 50 ppm at the stack. Stoichiometry and typical preliminary NO data for the test are shown on Figure 33.

LMF1C-3 AND 4 TESTS

The primary objectives of these tests were to: 1) obtain stable coal flow; 2) determine NO levels at 23 MW_t input with stoichiometries of 0.83 and 0.92 and N₂/O₂ ratios between 0.4 and 1.0, 3) determine NO levels at 28 MW_t with a stoichiometry of 0.85 and 0.92 and N₂/O₂ ratio of 0.7. The condition requiring the N₂/O₂ ratio of 1.0 resulted in a high combustor pressure, and it was not possible to provide adequate pressure in the coal feed tank to achieve a coal flow rate of 1.14 lb/s.

NO_x data for a thermal input of about 29 MW and a N₂/O₂ ratio of 0.7 are shown on Figure 34. This figure shows two curves to demonstrate the strong influence of stoichiometry, which was set at 0.82 in one case and 0.95 in another. The data for these tests and subsequent tests in the LMF1 series will be analyzed in depth and will be included in later test and topical reports.

Work continued toward improving the sample probes to reduce problems from pluggage or make provisions to remove pluggage during operation and from water carryover from the Perma-Pure dryers used in the commercial gas

Figure 30. Particulate Size From Cross-Over Duct During Tests LMF1C-3 and LMF1C-4

Figure 31. LMFlC-2 Flow Train

Figure 32. NO_x and Temperature along length of LMF1 Flow Train during LMF1C-2 Test
 Aug. 24, 1982 Time: 1910 hrs./ N/O: .4, Stoichiometry: 77%, Thermal Input: 26 MW_t

FIG. 33a.
STOICHIOMETRY
(DATA AVERAGED OVER 2 MINUTES)

FIG. 33b.
NO AT DIFFUSER EXIT
LOCATION 1
(DATA AVERAGED OVER 3 MINUTES)

FIG. 33c.
NO IN RADIANT FURNACE
LOCATION 2

FIG. 33d.
NO IN RADIANT FURNACE
LOCATION 4

FIG. 33e.
NO IN RADIANT FURNACE
LOCATION 5

FIG. 33f.
NO_x AT SECONDARY
COMBUSTOR OUTLET
LOCATION 7

FIG. 33g.
NO IN STACK
LOCATION 8

Figure 33. LMF1C-2 NO_x Levels

Figure 34. NO_x and Temperature along LMF1 Flow Train during LMF1C-4 Test September 17, 1982

conditioning systems. One probe was modified to permit cleaning of slag deposits during operation, additional heating tapes were added and the dryers were cleaned and checked for leaks, and automatic purge capability was added. Capability was developed for inhouse repair of the dryers. Although these changes have resulted in longer, more dependable sampling periods, significant additional work is required to provide long term continuous operation of the gas analysis equipment.

REFRACTORY MATERIALS EVALUATION

Refractory components of the CFFF flow train were visually inspected after each test during this quarter, and the majority of the refractory exhibited little change from previous tests. Areas which did exhibit increased degradation were the primary slag screen and the drum module. The primary slag screen continued to have a primary and a secondary wear area as described last quarter, and repairs between tests were attempted in an effort to reduce degradation in this area. Figure 35 shows a repair to the primary wear area prior to LMF1C-3 and consists of two 90% Al_2O_3 /10% Cr_2O_3 brick which are each 228 mm x 114 mm x 38 mm thick. These brick were attached to the slag screen with four small bolts, then surrounded with a 65% Al_2O_3 plastic which contains some SiC aggregate. As a final step, the bolt heads were covered with an alumina-chrome oxide mortar to help reduce oxidation of the bolts and allow the plastic refractory to achieve a bond and hold the bricks to the slag screen. During LMF1C-3, this repair was observed with a television camera. Cracks developed quickly in the bricks, due to the thermal shock created by starting coal combustion, or due to the propagation of cracks already present from the impact drilling of the holes in the brick. Within twenty minutes after coal combustion had started, pieces of the brick detached from the slag screen and one was later recovered from the slag hopper. This piece was 25 mm thick and thus had resisted immediate degradation by the plasma. The hot face of the brick showed preferential erosion of the matrix phase, with individual Al_2O_3 grains visible on the surface and a very thin (about 1 mm) coating of slag on the surface. Slag screen repairs with brick patches will be tried again in future tests. The high alumina plastic surrounding the brick was not in evidence at the conclusion of the test, as was true for repairs made with a 90% Al_2O_3 - 10% Cr_2O_3 plastic refractory after LMF1C-4.

The insulating castable lining of the drum module exhibited increased spalling which was evident in all parts of the drum module. This was likely due to a reaction between the potassium in the gas stream dust and the refractory, as preliminary microscopic examination has revealed significant amounts of potassium on the refractory surface. However, this investigation is not complete and no firm conclusions can be drawn. The refractory test door (Figure 36) as well as a 1.5 m by 1 m panel on the east wall of the drum module exhibited melting of the refractory after LMF1C-4, likely due to exceeding the melting point of the refractory, as temperatures in this area may have exceeded 1813K (2803°F). Since the refractory in these two areas was only rated for use to 1643K (2497°F), melting could have occurred and could have been accelerated by the fluxing effect of the potassium present on the refractory surface. The chemical content of the melted refractory was different from that of the remaining drum module refractory which was rated for use to 1698K (2596°F) resulting

Figure 35. Alumina-Chrome Oxide Bricks Surrounded By
a 65% Al_2O_3 Plastic

Figure 36. Insulating Castable Refractory on
Material Test Door in Drum Module
After LMF1C-4

in the different performance of the two materials. A more suitable refractory composition is being sought for the drum module, but the lining does not need to be repaired immediately.

Cracking and spalling of the refractory on the superheater flange door in the ash/seed hopper was also observed this quarter, probably due to water leaks observed in the door itself. This damage was repaired with two types of insulating castable, but exhibited cracking after LMF1C-5. This cracking is thought to be due to deformation and movement of the door when water pressure is applied, as some of the spacers inside the door have become detached and allowed the door to move. This condition is not considered serious and there are no plans to correct it.

METALS EVALUATION & ASH SEED DEPOSITION

Testing of the Materials Test Module (MTM) continued in the CFFF during this quarter. The objective is to investigate the extent and nature of ash/seed fouling of MHD superheater and intermediate temperature air heater tubes and the corrosion of tubes beneath these deposits. The MTM initially contained 34 pendant U-shaped 25.4 mm (1 in.)-O.D. tubes of the alloys given in Table V located immediately downstream of the radiant furnace.

The unit has been included in nine tests for a total time on coal of 15.0 hours with 19.5 additional hours on fuel oil only. The two rows of U-tubes were connected in parallel for the first 5 runs and in series for the later runs. Gas temperatures averaged 1255K (1800°F) with excursions to about 1477K (2200°F). With the series arrangement, metal temperatures at the air inlet averaged 789K (960°F) and at the outlet averaged 1116K (1550°F). However, excursions as high as 1366K (2000°F) occurred because of the limited amount of air (0.5 kg/s) available for cooling.

Only one run, LMF1C-2, was made this quarter with the MTM included. During this test, the maximum air flow available was only 0.35 kg/s (0.78 lb/s). As a result, tube temperatures exceeded 1366K (2000°F) and most of the tubes were destroyed. Figure 37 shows the upstream and Figure 38 the downstream tube appearance. Deposits were much thicker than in previous tests, and of non-uniform thickness. Irregular protrusions of deposits existed on some tubes 25 mm (1 inch) or more thick. Deposits were thickest and least adherent on the most corroded tubes. This is probably related to the thicker, non-adherent scales produced. It appears that the lowered heat transfer caused the deposit surface temperature to exceed the melting point and the molten surface collected impinging ash and seed particles to a greater extent than did solid surfaces. Deposit "bumps" were found at locations of locally intense corrosive attack, some of which penetrated the tube wall. In several locations, yellowish streams of molten K_2SO_4 were found to have run down the tubes. Back side deposits, as in prior runs, were powdery or lightly sintered and fell off at the slightest disturbance.

Upstream deposits on 304 and 316 stainless steel and Inconel 600 tubes from LMF1C-2 were examined by SEM/EDAX. The stainless steel tube deposits had two distinct layers, each of about equal thickness. The outer layer,

TABLE V
AIR HEATER TEST MATERIALS

Material/Specification	% C	% Mn	% P	% S	% Si	% Cr	% Ni	% Mo	Other
Croloy 2-1/4* A335 P22	0.15	0.30-0.60	0.030	0.030	0.50	1.90-2.60	--	0.87-1.13	--
Croloy 9* A335 P9	0.15	0.30-0.60	0.030	0.030	0.25-1.00	8.00-10.00	--	0.90-1.10	--
304 S.S.* A213 TP304	0.08	2.0	0.045	0.030	1.0	18-20	8-10.50	--	--
316 S.S.* A213 TP316	0.08	2.0	0.045	0.030	1.0	16-18	10-14	2.0-3.0	--
321 S.S.* A213 TP321	0.08	2.0	0.045	0.030	1.0	17-19	9-12	--	0.4 Ti(min)
446 S.S.* A258 TP446	0.20	1.5	0.040	0.030	1.0	23-27	--	--	0.25 N
310 S.S.* A312 TP310	0.25	2.0	0.045	0.030	1.5	24-26	19-22	--	--
26-1* A268 XM-27	0.01	0.40	0.02	0.02	0.40	25-27.5	0.50	0.75-1.50	0.015 N
Inconel 600 SB-167	0.08	0.50	--	--	0.20	15.5	76.0	--	8.0 Fe 0.20 Cu

*Balance Fe

Figure 37. LMF1C-2 Upstream MTM Tube Appearance

Figure 38. LMF1C-2 Downstream MTM Tube Appearance

Figure 39a, consisted of sintered ash with isolated islands of K_2SO_4 . The ash and seed were segregated rather than intimately mixed as one might expect. The outermost portion (about 1 mm thick) had no K_2SO_4 at all. The fly ash contained a high concentration of potassium. The inner layer (Figure 39b) was almost entirely K_2SO_4 with some iron and chromium oxide from the tubes.

The Inconel 600 tube deposits were identical to those of the stainless steel with respect to the ash and K_2SO_4 distributions but the concentration of tube metal elements was greater. Many spheres of nickel oxide (Figure 40) up to 40 μ in diameter were present in the inner portion of the deposit, some with an outer layer of NiS. Farther out at about the middle of the deposit, these were either all NiS or NiS with an outer layer of NiO. Dispersed throughout the K_2SO_4 or ash matrix was a second phase (marked with a circle on Figure 40) consisting of chromium, iron and nickel oxides in order of decreasing concentration. The outer deposit contained only ash and isolated patches of K_2SO_4 .

Deposits removed from MTM tubes after LMF1C-1 were also examined this quarter. The only difference in structure from that described for LMF1C-2 was that segregation of K_2SO_4 and fly ash was less, perhaps due to the lower deposit temperatures. While the K_2SO_4 content increased toward the tube, there was still appreciable ash in the inner portion. This ash was sintered together even near the tube surface. Figure 41 shows deposited ash and seed at the outer surface which had had insufficient time to sinter. Ash spheres up to about 16 μ in diameter were present along with K_2SO_4 particles averaging 2-3 μ diameter. The downstream deposit material shown in Figure 42 was entirely 5-10 μ K_2SO_4 with a hexagonal platelet structure.

Materials probes were installed in the drum module and secondary combustor outlet during LMF1C-2 to further investigate tube fouling and corrosion. However, sufficient nitrogen coolant flow was not available to maintain the desired temperatures. Process air will be used as a coolant in continued testing with the probes next quarter.

CFFF - HEAT TRANSFER EVALUATION

During this quarter downstream heat loss data were analyzed for tests LMF1C-2, 3, and 4. The data were time-averaged over two-minute intervals. In addition, a new time-averaging downstream data reduction code was developed. Data parameters selected for analysis included all gas temperature measurements (both optical and thermocouple) from the diffuser to the secondary combustor, heat loss from independently-cooled access doors and ports in the radiant furnace, and the bulk heat loss from the radiant furnace.

A number of cases of interest have been selected for analysis in the LMF1C test series. In order to obtain the gas thermodynamic and transport properties the NASA SP-273 Chemical Equilibrium Code was used. Both primary and secondary combustion (i.e. air injection) cases were generated for input into the downstream heat transfer code.

a. Outer Portion

b. Inner Portion

Figure 39. Cross-Section of Ash/Seed Deposit,
Front Side, Tube A-10, LMF1C-2

Figure 40 . Tube-Derived Metal Oxide/Sulfide Phases
in Inconel 600 Ash/Seed Deposit

Figure 41. Fly Ash and Seed on Front Deposit
Surface LMF1C-1

Figure 42. K_2SO_4 Back Side Deposit, LMF1C-1

The downstream code was modified to allow calculations to extend through the secondary combustor and into the ash/seed hopper to the centerline of the crossover duct. At the axial location where secondary combustion occurs, the flame temperature calculated by the SP-273 Code is imposed as the gas temperature.

During the next quarter comparisons of experimental and analytical gas temperature distributions will be made for all the cases of interest in the LMFIC test series. Preliminary results obtained thus far indicate matching within 10%. Further refinement of the heat transfer code is in progress. The gas temperature distributions obtained will also be used as input for NO_x relaxation calculations.

SECONDARY COMBUSTOR MODELING

Analysis of data obtained in the secondary combustor cold flow model was completed this quarter and a Topical Report on the results of this study was drafted. The 1/6 scale cold flow model used water as the working fluid to experimentally obtain a high Reynolds number. The model was constructed to permit independent variation of, 1) the angle between the air injectors and main stream, 2) the velocity ratio between the injected and primary streams, and 3) the cross-sectional area of the downstream restriction. In the study secondary flow injection angles of 90° and 120° (relative to the bulk flow direction), velocity ratios of 20/1 and 10/1, and cross-sectional area reductions of 50%, 25% and 0% were tested. The conclusions resulting from the study include:

- 1) A bulk velocity ratio of 20 between the secondary injection and primary stream provides an axially shorter, more intense mixing region with more uniform mixing across the duct than a velocity ratio of 10. Furthermore, for the velocity ratio of 20 case the mean flow was symmetrically distributed across the duct, while for the velocity ratio of 10 case the mean flow always followed either the top or bottom wall. Therefore a velocity ratio of 20 would be preferable to a velocity ratio of 10 for secondary combustor operation.
- 2) For the cross flow injection type combustor considered, test results did not indicate clear-cut advantages for either 120° or 90° injection. Injection at 120° results in greater turbulence near the walls while 90° injection provides greater turbulence near the duct centerline; hence, 90° injection could provide a less severe environment for the walls of the combustor. However, the model test results do not support a firm conclusion in this regard.
- 3) An area reduction downstream of the secondary air injection shortens the downstream recirculation. A 25% area reduction causes some shortening but a 50% area reduction eliminates recirculation downstream of the restriction, and would probably allow for a shorter combustor with greater energy transfer to the superheater section located downstream of the secondary combustor.

- 4) In all cases studied the flow had a periodic wake regime, resulting from the alternate growth and shedding of vortices produced by the interaction of the injected and bulk flows. Strong oscillations with frequencies from 0.7 to 1.6 Hz were identified, but the phenomenon controlling the shedding frequency was not established (although some dependence on the duct area reduction was indicated.)

It has been shown that data from water flow models, scaled (as in this study) to match the bulk secondary to primary velocity and mass flow rate ratios, agree well with real combustor performance data. Therefore, conclusions from the cold flow model study should provide useful insights into the CFFF secondary combustor operation, provided that due consideration is given to the effects of density gradients with the density change produced by chemical reactions and the evolution and transfer of heat, which cannot be simulated with cold flow modeling.

4.6 Diagnostic Support

Diagnostic activities this quarter primarily involved instrumentation installation, set up and pre and post-test calibrations for CFFF testing. Diagnostic support for cold flow modeling of the primary and secondary combustors was also provided. Coal flow instrumentation, smoke detectors, flame detectors, and the wide band data recording system were routinely set up for these tests. However, test support efforts were concentrated on spectroscopic temperature measurements of the high temperature gas upstream of the LMF top drum module and secondary combustor. Luminosity and line reversal temperature measurements and spectral emission/transmission measurements were made using diagnostics systems developed at UTSI for the CFFF. Attempts were also made to measure wall temperatures, wall emissivities and gas temperatures using the Mississippi State University (MSU) developed diagnostics. Unfortunately, the MSU diagnostics were deleteriously impacted by unanticipated gas emission characteristics. MSU is modifying their diagnostics to overcome their difficulties.

Relative temperature distributions were measured for the LMF1 tests at the nozzle throat and the radiant furnace, and typical spatial distribution are presented in Figures 43 and 44 for the LMF1 base case. The luminosity temperature profiles at the nozzle throat indicate a cooler gas core surrounded by hotter gas. Unfortunately, luminosity measurements give gas temperatures "averaged" over the gas seen by the probe, and optical depths are not known for plasma in the nozzle throat. Therefore the centerline luminosity measurement may only be "averaging" over the thermal boundary layer region at the centerline while the near wall measurements may be including effects from a hot gas core obscured from the centerline probe. Thus it is not possible to conclude that plasma in the center of the nozzle is cooler than plasma nearer the walls. Likewise since nozzle luminosity measurements made prior to initiating the LMF1C test series do not generally indicate such a cool core as the LMF1C-3 and LMF1C-4 data, and some wear of the nozzle walls have been observed, the cooler centerline plasma temperatures measured during LMF1C may be attributable to alterations of the optical access ports rather than to a plasma condition. This problem will be examined further in future testing.

Figure 43. Temperature Profiles Across LMf1 Nozzle Throat

Figure 44. Temperature Profile Ahead of Slag Screen for LMf1 Base Conditions (LMf1C-4, Step 7)

More definite conclusions can be drawn from the measured luminosity profiles in front of the slag screen, shown in Figure 44. Because spectroscopic transmission measurements made across the diffuser exit centerline (as typified by Figure 45 for LMF1C-3, step 9) indicate transmission across the entire optical path for the 750 mm luminosity measurement wavelength the luminosity probes in front of the slag screen will "average" emissions across the entire furnace width. Thus, as long as slag does not plug the ports, the luminosity temperature profiles in front of the slag screen should indicate a "bulk" gas temperature distribution. Considering this, Figure 44 shows that the hottest portion of the gas from the diffuser is passing through the slag screen along the highest possible path. Therefore, gas samples extracted from the gas sample probe just below the slag screen may not be indicative of gas composition for the diffuser exit gas.

Figure 45. Plasma Transmission at the Diffuser

During the LMF1C-2, -3 and -4 testing slagging problems occasionally prevented continuous luminosity and line reversal temperature measurements. It appears that the cool nitrogen purge gas is cooling slag around the optical access holes, and long slag "tubes" were observed at several luminosity ports after each of the LMF1C tests. Attempts will be made next quarter to pulse purge the luminosity ports periodically generating heat to melt any slag formations. Equipment has been ordered for a trial system, but delivery is not expected until near the end of next quarter.

The sensitivity and uncertainty limits for both the line reversal and luminosity systems were examined this quarter between LMF1C testing. This examination indicated that relative luminosity temperatures are generally 2% to 3% uncertain ($\sim 50\text{K}$) and are usable for temperature measurements down to around 1150K (1600°F), if sufficient potassium remains in gas phase at these temperatures. Absolute luminosity temperatures are, of course, considerably less accurate since the gas emissivity is unknown. Comparisons of luminosity and line reversal measurements at the diffuser exit indicate, for the LMF1C-3 and LMF1C-4 tests, that luminosity measurements are

generally 4% to 6% (100K to 150K) lower than the absolute gas temperature, if it is reasonable to assume a uniform temperature, particulate-free gas in front of each luminosity probe. Absolute line reversal temperature measurements are around 1% to 3% (25K to 75K) uncertain. Again the non-isothermal character of the gas and particulate light scattering, absorption and emission levels will introduce additional uncertainty in the "average" absolute line reversal temperature measurements. The present UTSI line reversal system should be able to measure gas temperatures down to 1500K (2300°F) with system optimization for these measurements. Gas temperatures between 1550K to 1700K were measured at the top of the radiant furnace during the LMFIC-1 test, so these estimated lower sensitivity limits appear reasonable.

Evaluation of a temperature profile measuring line reversal system continued, and has led to the design of a photodiode configured line reversal system. This photodiode system has the potential of greatly reducing the size and complexity of spectrometer configured line reversal systems, as presently employed, and will, therefore, be tested during the upcoming LMFIC tests. Additional components for another spectrometer configured line reversal system were obtained this quarter, but assembly of that system will not be carried out unless the photodiode system proves inadequate.

Cold flow model studies in support of combustor and secondary combustor developments (Subtasks 4.1 and 4.3) continued. A detailed uncertainty analysis and statistical confidence analysis was performed for the laser velocimeter (LV) measurements made in the 1/6 scale model of the secondary combustor. Typical statistical confidence tolerances are presented in Table VI. In Table VI $\bar{u}$ represents the axial mean velocity, $\bar{v}$ the vertical mean velocity, u' the standard deviation of the axial velocity fluctuation, and v' the standard deviation of the vertical velocity fluctuations. In addition, the LV system has been installed and set up for measurements in the water flow model of the primary combustor. A schematic showing the optical configuration for this study is shown in Figure 46. Preliminary results from this study indicate that considerably larger simultaneous sample sets will be required to attain statistical confidence tolerances in the primary combustor measurements that are similar to those obtained in the secondary combustor. The necessary changes will be made to the data acquisition and reduction software, and the baseline testing will be completed next quarter for the primary combustor study.

D. TASK 5 - TESTING OF DOE SUPPLIED COMPONENTS

There is no activity to report.

E. TASK 6 - MODIFICATIONS TO THE CFFF

There is no activity to report.

F. TASK 7 - TECHNOLOGY DEVELOPMENT PROGRAM

There is no activity to report.

TABLE VI

TYPICAL LASER VELOCIMETER MEASUREMENT 95% CONFIDENCE LIMITS

AXIAL				VERTICAL			
$\bar{u}$ m/s	$\Delta\bar{u}(\%)$ tolerance $\pm$	u' m/s	$\Delta u'(\%)$ tolerance $\pm$	$\bar{v}$ (m/s)	$\Delta\bar{v}(\%)$ tolerance $\pm$	v' (m/s)	$\Delta v'(\%)$ tolerance $\pm$
-0.741	4.0	0.545	3.8	1.367	1.8	0.439	4.3
-0.068	25.0	0.314	3.2	-0.575	3.7	0.398	3.8
-0.511	2.2	0.215	12.5	-0.050	38.0	0.364	0.3
0.492	4.1	0.368	2.4	1.614	1.4	0.544	2.6
0.637	1.7	0.210	3.8	-0.039	43.6	0.318	3.1
0.070	17.1	0.224	4.0	0.203	16.7	0.618	2.8
0.226	7.5	0.317	3.8	0.746	3.2	0.451	6.2
0.318	4.7	0.281	3.6	0.305	4.6	0.260	5.4

 $-1.76 \text{ m/s} < u < 1.53 \text{ m/s}$ $-4.47 \text{ m/s} < v < 4.47 \text{ m/s}$

Figure 46. LV Optical Train for the Primary Combustor Flow Study

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TOPICAL REPORT STATUS

TOPICAL REPORTS SUBMITTED TO DOE IN FINAL FORM

FE-10815-63, "High Temperature Combustion of Pulverized Coal."

FE-10815-76, "Coal Injectors Performance Study."

PROJECTED REPORTS

Foote, J., et al., "Study of Flow Field and Mixing in the Secondary Combustor with Cold Flow Modeling."

Galanga, F. L., "A One-Dimensional Code To Predict the Thermal Behavior of the UTSI MHD Radiant Furnace."

APPENDIX A

PRESSURES AND HEAT FLUXES FOR TESTS
LMF1C-3 AND LMF1C-4 PLOTTED VERSUS
FLOW TRAIN DISTANCE FROM
CHANNEL ENTRANCE

ACCUMULATED LMF1 TEST RESULTS
MDOT=2.94 kg/s (6.47 lb/s) STOICH=.32 N/O=.4
UPSTREAM PRESSURE DISTRIBUTIONS

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LMF1C-3
STEP NO. 5

ACCUMULATED LMF1 TEST RESULTS
MDOT=2.94 kg/s (6.47 lb/s) STOICH=.32 N/O=.4
UPSTREAM HEAT FLUX DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
MDOT=2.85 kg/s (6.28 lb/s) STOICH=.94 N/O=.41
UPSTREAM PRESSURE DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
MDOT=2.85 kg/s (6.28 lb/s) STOICH=.94 N/O=.41
UPSTREAM HEAT FLUX DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
MDOT=3.41 kg/s (7.50 lb/s) STOICH=.92 N/O=.73
UPSTREAM PRESSURE DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
MDOT=3.41 kg/s (7.50 lb/s) STOICH=.92 N/O=.73
UPSTREAM HEAT FLUX DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
 MDOT=3.55 kg/s (7.80 lb/s) STOICH=.89 N/O=1.0
 UPSTREAM PRESSURE DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
MDOT=3.55 kg/s (7.80 lb/s) STOICH=.89 N/O=1.0
UPSTREAM HEAT FLUX DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
MDOT=3.12 kg/s (6.87 lb/s) STOICH=.84 N/O=0.7
UPSTREAM PRESSURE DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
MDOT=3.12 kg/s (6.87 lb/s) STOICH=.84 N/O=.70
UPSTREAM HEAT FLUX DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
MDOT=3.64 kg/s (3.00 lb/s) STOICH=.83 N₂O=1.02
UPSTREAM PRESSURE DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
MDOT=3.64 kg/s (8.00 lb/s) STOICH=.83 N/O=1.02
UPSTREAM HEAT FLUX DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
MDOT=3.86 kg/s (8.50 lb/s) STOICH=.91 N/O=1.0
UPSTREAM PRESSURE DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
MDOT=3.86 kg/s (8.50 lb/s) STOICH=.91 N/O=1.00
UPSTREAM HEAT FLUX DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
MDOT=3.85 kg/s (8.46 lb/s) STOICH=.82 N/O=0.7
UPSTREAM PRESSURE DISTRIBUTIONS

LMF1C-4
STEP NO. 7

ACCUMULATED LMF1 TEST RESULTS
MDOT=3.85 kg/s (8.46 lb/s) STOICH=.82 N/O=.70
UPSTREAM HEAT FLUX DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
MDOT=4.14 kg/s (9.12 lb/s) STOICH=.94 N/O=.71
UPSTREAM PRESSURE DISTRIBUTIONS

ACCUMULATED LMF1 TEST RESULTS
MDOT=4.14 kg/s (9.12 lb/s) STOICH=.94 N/O=.71
UPSTREAM HEAT FLUX DISTRIBUTIONS