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KEY CONTRIBUTIONS IN MHD POWER GENERATION

Semi-Annual Report, December 1, 1978—May 31, 1979

By
J. F. Louis

MASTER

June 1979
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Massachusetts Institute of Technology
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U. S. DEPARTMENT OF ENERGY

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SEMI-ANNUAL REPORT

for the period

1 December 1978 - 31 May 1979

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Date Prepared - June 1979

PREPARED FOR THE UNITED STATES
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TABLE OF CONTENTS

ABSTRACT	v
I. OBJECTIVE AND SCOPE OF WORK	1
II. SUMMARY OF PROGRESS TO DATE	3
III. DETAILED DESCRIPTION OF TECHNICAL PROGRESS	14
Task 1. Arcing Phenomena in MHD Generators	14
Task 2. Open Cycle MHD Disk Generator Program	125
Task 3. Electrode Module Development and Testing	229
Task 4. Coal Combustion Studies	325
Subtask 4a. Kinetic Modelling of 2-Stage Combustors	325
Subtask 4b. Combustion Aerodynamics and Fluid Mechanics of High-Swirl Second Stage	351
Task 5. Project Administration	
IV. CONCLUSIONS	356

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ABSTRACT

Activities during the first six months of the contract period are reported both in summary and in detail. The tasks reported on include:

1) investigation of electrical behavior in the vicinity of electrode and insulating walls; 2) studies of critical performance issues in the development of combustion disk generators; 3) development and testing of electrode modules, including studies of insulator properties; and 4) determination of coal combustion kinetics and ash behavior relevant to two-stage MHD combustors, and investigation of the mixing and flow aerodynamics of a high swirl geometry second stage.

Experimental investigations of the mechanisms of current transport to clean (non-slugged) electrodes are discussed. Voltage drops on anode and cathode walls have been measured at average current densities up to 2.3 A/cm^2 with plasma and electrode wall conditions appropriate for commercial scale generators. With stainless steel electrode surface, no transverse arcs were observed on anodes, but cathode arc spots were seen for all but the lowest currents. Measured anode voltage drops were compared with values predicted by a boundary layer code. Additional work carried out in preparation for continuation of the clean-wall experiments and on associated modeling is also presented. The status of slagged-wall related work is reported; this includes studies of mechanisms of conduction through slag, and studies of the formation and behavior of ripples on thin viscous films.

Studies of the disk configuration have focussed on the experiments to investigate the effects of plasma nonuniformities due to wakes from inlet guide vanes. Work accomplished to date has been concerned with establishing reference flow conditions for a radial outflow disk with zero inlet

swirl and no wakes. Results obtained have been compared with theoretical flow behavior for Ar-CO and Ar-CO₂ mixtures, with effective Hall coefficients up to 4.5 in each case. Design of the struts to be used for creation of wake nonuniformities is discussed, and analytical work in support of these experiments is reviewed. Other experiments on the propagation of large scale nonuniformities have been carried out, as part of the program to develop improved diagnostics for the experimental facility. Improvements to the laser interferometer are also discussed.

The ternary systems SrZrO₃-LaFeO₃-SrFeO_{3-δ} and SrZrO₃-LaFeO_{0.5}Cr_{0.5}O₃-SrFeO_{0.5}Cr_{0.5}O_{3-δ} were investigated as possible electrode materials. Phase diagrams were completed and electrical conductivity and Seebeck voltage measurements were made over a wide range of oxygen partial pressures. Dielectric breakdown measurements on four materials are reported. Single-layer electrodes of SZSFLF-3 were tested at 1800°C surface temperature and 0.5 A/cm² for 15 hours, 15 minutes, with total test time exceeding 17 hours. Multilayered electrodes of graded conductivity and refractoriness were tested at 1800°C surface temperature and 1 A/cm² for 11 hours and 53 minutes, with total test time exceeding 13 hours.

Ash vaporization kinetics have been determined over a temperature range of 2000 to 3000°K, and included in the combustor system model. Literature data on the CO₂-C, H₂O-C, OH-C, and O-C reaction mechanisms have been used to refine the char combustion equations and the improvements have been added to the system model. The status of the water-model combustor test facility is reported, and the parallel modeling effort is described.

OBJECTIVE AND SCOPE OF WORK

The Department of Energy (DOE) is responsible for a national program for the development of commercial open-cycle MHD power generation with the initial objective of establishing the engineering feasibility of MHD power systems utilizing coal or fuel derived from coal.

Research and development in open-cycle coal-fired MHD power generation has been underway at Massachusetts Institute of Technology (MIT) since 1971, the scope of the effort gradually expanding to its present level. The scope and objectives of the present MIT program are as follows:

1. To carry out an experimental investigation of the mechanisms of near-electrode voltage drops, axial breakdown, onset of transverse arcs, arc damage, slag shorting and related phenomena important for the design of MHD electrode and insulating walls, and to develop analytical models in support of these experiments.

2. To investigate, experimentally and analytically, critical performance issues involved in the development of combustion disk generators. Particular emphasis will be given to determination of the effective plasma properties and generator performance as affected by (a) plasma nonuniformities, (b) chemical nonequilibrium, (c) unsteady flow phenomena, (d) boundary layers and slag, and (e) inviscid flow distortion.

3. To perform the engineering design, construction and testing of electrode modules. This work will include: (a) a laboratory-based materials selection investigation, (b) electrode/insulator fabrication, and (c)

performance measurements. In addition, a model of the time-dependent performance based on measured module data will be developed. The major emphasis of the task will be on development of super-hot electrode modules and measurement of dielectric breakdown of insulator materials and coal slag.

4. To research the coal combustion kinetics relevant to the two-stage MHD combustor, focussing on two problem areas: (a) coal oxidation kinetics under fuel-rich conditions, and (b) ash behavior in an MHD combustion environment. This task will also include study of the mixing and flow aerodynamics behavior in a high swirl geometry intended for the second stage of an MHD combustor, using a simple water model test facility.

II. SUMMARY OF PROGRESS TO DATE

TASK 1. ARCING PHENOMENA IN MHD GENERATORS

The principal experimental results obtained in this reporting period relate to the mechanisms of current transport to clean (non-slugged) electrodes. Voltage drops on anode and cathode walls were measured at average current densities as high as 2.5 A/cm^2 at plasma and electrode wall conditions similar to those of a commercial scale MHD generator. Visual observation of the stainless steel electrode surface indicated the absence of transverse arcs on anodes for all conditions and showed cathode arc spots for all but the lowest currents. Measured anode voltage drops were compared with values predicted by a boundary layer code. In addition to solving the continuity, momentum and energy equations for the heavy gas, the program solves the continuity equation for the electrons, including the effects of electron-ion kinetics. Electron temperature elevation was estimated, but not included in the detailed calculation. Results of these comparisons are presented in the progress report and provide a guide for the continued efforts in this subtask. Preparations for a second experiment have progressed significantly.

Work relating the clean wall axial breakdown has centered on evaluation of an electrode wall configuration designed to alleviate the problem of insulator initiated axial breakdown. Calculation of the plasma side heat transfer characteristics for the composite wall construction has been completed and thermal modeling of the wall itself initiated.

Progress has been made in the assembly of an experimental rig to study the mechanisms governing damage due to interelectrode arcs and to characterize quantitatively the damage to specific electrode wall configurations. All major components, except a high voltage power supply for initiating

arcs in the inter-electrode insulator, have been procured and a major part of the installation completed.

Slagged-wall related work has progressed in two areas: a) the slag polarization effects, and b) the structure and effects of ripples on slag flow. A literature survey has been conducted, on the conduction mechanisms through molten slag, and the effects associated with it. A discussion is included of the likely prevailing mechanisms, and a simplified electron-hole-positive ions-negative ions model is described which shows the effect of oxygen potential on these mechanisms. Experiments have been initiated, with only low-temperature, solid state results obtained so far. An experimental and analytical study of the formation and behavior of ripples on thin viscous films has been initiated. Preliminary experimental results were obtained on an initial rig which simulates the slag flow by a flow of glycerine. Ripple patterns similar to those obtained for slagged-wall channels were observed in the simulation rig. Ripple characteristics, ripple height, speed, and wavelength were measured visually and correlated using dimensional considerations. Designs of an improved rig which will allow closer simulation of MHD slag flow conditions (including the viscosity gradient) has been completed and the new rig partially assembled.

TASK 2. OPEN CYCLE DISK GENERATOR PROGRAM

The potential benefits of the disk generator with swirl (as an alternative to a linear generator) are discussed, and earlier/previous results are reviewed. Critical performance factors/issues are defined, and the MIT disk program goals which addresses these issues are outlined. In particular, the effective plasma properties and generator performance (with and without swirl) as affected by (a) plasma nonuniformities, (b) electric stress limitations, (c) chemical nonequilibrium (in small-scale supersonic generators), (d) unsteady flow phenomena, (e) boundary layers (and inlet boundary layer separation) and slag, and (f) inviscid flow distortion are to be investigated. Theoretical modeling and the development of diagnostics are designed to support the experimental work; a detailed data base is to be developed which can be applied to the design and performance predictions of large-scale disk generators.

During the last six months our major efforts have been directed primarily in three areas: (a) nonuniformities studies in a radial flow disk, (b) instrumentation and diagnostics development, and (c) two-dimensional modeling of disk channels. The results are summarized as follows:

(a) Nonuniformities Studies

Experiments are being conducted in a radial flow generator in order to quantify thermal and velocity wake nonuniformity effects on generator performance. The main goal is to establish experimental reduction formulae for this type of nonuniformity, and consequently assess the validity of the numerical 2-D wake models. Two regimes/cases are being studied: (1) a weak interaction with the flow characterized by slow electron kinetics, and (2) a moderate interaction case where the free-stream electrons are in Saha equilibrium; emphasis is placed on the latter. For each case the experi-

ments are carried out in two phases. First, the reference or undisturbed flow is determined/characterized and compared with 1-D calculations which include the effects of finite-rate electron kinetics (primarily for the first case) and stability with respect to ionization. The reference flow should be as free as possible from background nonuniformities. Once this is accomplished, experiments with wakes will be made. The first phase is near completion; the progress and key results are outlined as follows:

1. A two-temperature quasi 1-D model originally developed at MIT for non-equilibrium (thermal) generator studies was modified for use with molecular gases. The basic model, which includes finite-rate electron kinetics and energetics, is described in detail in Appendix A. The variation of the collisional loss factor δ with electron density was included in the expression for the critical Hall parameter (stability criterion), and a generalized three-body recombination rate constant for electron-cesium ion recombination with heavy particles as the third body was added. Other minor modifications were incorporated as well.
2. Reference flow tests were conducted and analyzed for several gases/gas mixtures and stagnation conditions. Based on the results, conclusions were drawn as to the base case channel configuration, test gas and conditions best suited for the wake studies. Briefly, a 70% Ar-30% CO mixture at stagnation conditions $T_0 = 2800$ K, $p_0 = 5.7$ atm (0.16% Cs seed concentration) provided a quasi steady uniform flow for nearly 1.5 msec. Effective Hall coefficients up to 4.5 were obtained as a result of the slow electron kinetics/plasma response to ionizational instabilities (stability calculations assuming instantaneous

development of ionizational nonuniformities predict a saturation at about $\beta_{\text{eff}} = 2$ for these conditions). For the equilibrium case, an initial composition of 80% Ar-20% CO_2 at stagnation conditions $T_0 = 2900 \text{ K}$, $p_0 = 6.5 \text{ atm}$ provided results in good agreement with the model (for the voltage and Hall parameter saturation). The generator is currently being modified in accordance with model (lowering the average Mach number to ≈ 1.3) which predicts a stable plasma up to $\beta_{\text{eff}} = 5.2$ at $B = 3.5 \text{ T}$. Further reference flow tests will be made for these conditions (with the modified channel) prior to tests with wakes. N_2 was ruled out as a major test gas component; the flow was highly nonuniform, primarily due to shock wave attenuation and bifurcation effects in the shock tube, resulting in nonuniform seed distribution.

3. The initial design of the aerodynamic profiles/struts which will be used to create the wake nonuniformities has been determined. A "spider" of flat-plate profiles (up to 50) approximately 0.38 mm thick will be used, and the first tests will focus primarily on the reduction in open-circuit voltage as function of the number of struts. Boundary layer profiles (with finite-rate electron kinetics) and the wake effect (using the 2-D uncoupled model) were calculated. Strong voltage reductions ($> 50\%$) were found for Hall parameters in the range $2.5 \leq \beta \leq 4.7$ when the number of wakes exceeded 25 (see Table III).

(b) Instrumentation and Diagnostics Development-

1. High-bandwidth A/D (analogue to digital) equipment was installed,

interfaced with the PDP-11/60 computer, and used with a cross-correlation routine to determine the orientation and propagation velocity of a large-scale (> 1 cm) nonuniformity (extension of the work in Appendix A). The equipment will be used primarily in conjunction with later work on small-scale nonuniformities, e.g. isotropic turbulence growth/decay studies.

2. Improvements in the laser interferometer system (for neutral gas density measurements) were made. These included RF shielding of the laser head and power supply, and installing non-metallic optical component mounts to minimize fringe distortion resulting from the magnetic force. A technique is outlined for increasing the sensitivity of the system for experiments with short optical path and weak density gradients.

(c) Two-Dimensional Modeling of Disk Generators-

1. Modifications of the electrodynamics (uncoupled) 2-D wake model were made and results for the wake experiment were obtained (see section (a)).
2. Small perturbation techniques are being studied for analyzing the effects of steady-state periodic inlet disturbances (nonuniformities) in the azimuthal direction. The linearized fluid dynamics equations and the corresponding electrodynamics equations have been formulated and included as Appendix B.
3. An analysis has begun for the study of boundary layer effects and the Hall-field breakdown (on the side walls) of disk generators. The the electrodynamics part of the problem has been developed for channels of constant height, and it is currently being tested for accuracy and finding a suitable relaxation parameter.

The program is approximately on schedule. Manpower has shifted slightly towards the radial nonuniformities study, and less emphasis was placed on the swirl experiment.

TASK 3, ELECTRODE MODULE DEVELOPMENT AND TESTING

PART A.

The ternary systems $\text{SrZrO}_3\text{-LaFeO}_3\text{-SrFeO}_{3-\delta}$ and $\text{SrZrO}_3\text{-LaFe}_{0.5}\text{Cr}_{0.5}\text{O}_{3-\delta}$ were investigated as possible electrode materials. Phase diagrams were completed and electrical conductivity and Seebeck voltage measurements were made over a wide range of oxygen partial pressures.

Development

Some dielectric breakdown measurements have been made at room temperature and elevated temperature for four materials.

Work has begun toward setting up a clean fired burner and associated equipment to test insulators with an imposed thermal gradient.

Testing

Single-layer electrodes of SZSFLF-3 were tested at 1800°C surface temperature and $1/2 \text{ A/cm}^2$ for 15 hours, 15 minutes. The total test time was 17 hours and 35 minutes.

Multilayered electrodes of graded conductivity and refractoriness were tested at 1800°C surface temperature and 1 A/cm^2 for 11 hours and 53 minutes. The total test time was 13 hours and 15 minutes.

Several new compositions were examined for improved electrical conductivity. Electrodes of a new composition designated SZSFLF-6 are being fabricated.

PART B.

The MHD Test Facility Complex

Several improvements were implemented in the MHD Facility, among which were: repiping indoors of the river water system; replacement of the city water pump with a new pump of higher pressure and higher capacity; temperature monitoring of the exhaust stream in the vicinity of the scrubber; better thermal protection for the cooling water hoses and control and instrumentation wiring; and addition of a fog spray to the exhaust fan.

A review of facility safety was initiated as a result of the untimely activation of the sprinkler system during a test.

An improved base plate and multi-hole seed injectors were replaced on the burner. Computer software was developed for rapid display of burner parameters, and an operational interface was designed for unfamiliar users. Ceramic electrodes were tested at $1/2$ and 1 amp/cm^2 for 17.6 and 13.25 hours respectively. A water cooled channel with interchangeable instrumentation was used in an extensive experimental program in an effort to define a correlation between heat transfer and friction coefficients in high temperature flows. A brick-lined channel with stainless steel electrodes was constructed and used in two diffused and arcing current transfer tests.

Specifications for a subatmospheric MHD generator were established and the design of key components and subsystems was initiated.

TASK 4, COAL COMBUSTION STUDIES

TASK 4a, KINETIC MODELLING OF 2-STAGE COMBUSTOR

The three subtasks involve pyrolysis, ash vaporization, and model development. In the pyrolysis experiments, the major effort has been expended on modifying the sample collection equipment in order to permit separation of soot from char in the pyrolysis experiments. The probe development has been completed approximately two months behind schedule. Pyrolysis experiments with the new probe are to be initiated during the next quarter. Ash vaporization kinetics have been determined over a temperature range of 2000 to 3000°K. in collaboration with a parallel program on an EPRI project. The data on ash vaporization kinetics and literature data on the $\text{CO}_2\text{-C}$, $\text{H}_2\text{O-C}$, OH-C , and O-C have been used to refine the char combustion equations and to include ash vaporization in the system model.

TASK 4b. COMBUSTION AERODYNAMICS AND FLUID MECHANICS OF HIGH-SWIRL SECOND
STAGE

The water-model combustor test facility and the first proposed model configuration have been designed and fabricated. Testing is expected to start in June 1979.

A parallel mathematical modeling effort is under way, with computer code generation in progress. Useful predictions should begin to be available in June 1979.

III. DETAILED DESCRIPTION OF TECHNICAL PROGRESS

TASK 1. ARCING PHENOMENA IN MHD GENERATORS

1. Introduction

Successful application of MHD power generation requires efficient and long-lasting channel wall designs. To effect such designs it is necessary to have a good understanding of the mechanisms of current transport in the vicinity of the wall and to develop models capable of predicting, for example, the conditions for which electrical arcing occurs. In addition it is important to develop techniques to determine the performance degradation from arcs and the extent of damage to the channel walls. The present task addresses the general problem of current transport to electrodes through a combined experimental and analytical study which includes problems specific to slagging wall operation and non-slagging wall operation. The initial studies, which are of a fundamental nature, are being performed without the complication of a magnetic field. However, the task also includes the designing of a sub-atmospheric channel and conventional magnet. This sub-atmospheric facility, described in Task 3, Part B, will allow extension of the experimental work to conditions typical of a full-scale generator operating at high Hall parameter.

The task is divided into several subtasks. Where possible, progress on each subtask is reported separately. The major experimental results obtained in this reporting period relate to current transport to non-slugged metal electrodes. These results were obtained in an experiment in the MHD simulation facility which studied anode and cathode operation at current densities up to 2 A/cm^2 . Section 2 of this report describes the apparatus and experimental results and discusses the comparisons of predicted anode wall voltage drops with the experimentally-measured values. Work relating to axial breakdown on non-slagging walls has included heat transfer

calculations for an electrode wall configuration designed to alleviate the problem of insulator breakdown. Preparations for the first experiment, planned for mid-August, have begun. These activities are described in Section 3 of the report. Considerable progress has been made in assembling the experimental rig in which the axial breakdown arc damage and control experiments will be performed. A conventional magnet and power source have been installed and the power supplies and other electrical circuitry for providing power for the arc discharge have been set-up and tested. Progress in this area is described in Section 4.

Slagged-wall related work has progressed in two areas: "polarization" of slagged electrodes and fluid behavior of slag flow with ripples. A literature survey has been conducted on the conduction mechanism through molten slag, and the effects associated with it. A discussion is included of the likely prevailing mechanisms, and a simplified electron-hole/positive ions-negative ions model is described which shows the effect of oxygen potential on these mechanisms. Experiments have been initiated, with only low-temperature, solid-state results obtained so far. These results and a brief description of the experimental apparatus are given in Section 5a.

The fluid mechanics of slag flow, in particular the formation and effect of transverse ripples, has been studied in a cold-air channel-flow rig with glycerine representing the slag. Results from this work will be applied to the problem of current transport once the fluid mechanics is better understood. Transverse ripples were observed in preliminary experiments and the variation of parameters describing the ripples with the appropriate dimensionless numbers was obtained. To more exactly match the conditions of the actual slag flow, a second rig has been designed and constructed. These activities are described in Section 5b.

In addition to the main experimental and analytical efforts several support activities have been conducted during this reporting period. Up-keep and upgrading of the MHD simulation facility is the major effort and is described in a separate chapter of the report. In support of the breakdown and arcing studies a device to measure spatially and temporally resolved temperature distribution in the vicinity of the electrodes has been developed. This work is described in Section 6a. Analytical work in support of each particular experiment is discussed with the experimental work; additional support analysis is discussed in Section 6b.

The final activity is the design of a subatmospheric MHD generator test facility. In this reporting period preliminary designs of the combustor, channel and magnet have been performed. This work is described in Task 3, Part B.

2. Diffuse Discharge and Onset of Arcing

An understanding of the mechanism of current transport to the electrodes in the MHD generator is important because of the role current transport plays as a loss mechanism (voltage drops, interelectrode shorting and arcing) and as a contributor to erosion and degradation of the channel walls. Recent detailed comparisons of voltage drops and boundary-layer profiles for combustion gas plasmas have indicated the need for further experimental and analytical work^[1,2]. Surface-plasma interactions are very poorly understood and arc-mode operation is not well modeled. The overall purpose of this subtask is to develop further understanding and predictive capabilities for the electrical discharge processes in the vicinity of the electrode surface. Particular attention is first directed toward the diffuse mode of current transport and to the conditions for which a diffuse discharge is no longer possible.

Progress discussed in this report includes one major experiment in the MHD simulation facility and analysis of the data. The principal objectives of this first experiment were a) to measure the anode-wall and cathode-wall voltage drops and to directly compare the measurements with available calculation codes, and b) to determine the current densities required to produce constricted discharges on the cathode and anode walls. Voltage probes were used to measure the potential distribution across the channel for a variety of current densities. Visual records of the electrode surface were obtained using a movie camera and a video camera; filters were used to accentuate the wall response and the plasma response. A boundary-layer code, originally developed by Daily^[9], was used to predict electron number density profiles and to compute the potential distribution at a given current density. The effect of electron-ion recombination rates was studied with the program. In addition to the main goals, combustor heat balance measurements and calculation procedures were checked and experience gained with the present channel type in the MHD simulation facility.

Preparations for a second experiment in the facility have advanced considerably in this reporting period and the main goals and conditions for this experiment are detailed. Expected extensions of the analytical work are also discussed.

2a. Experimental Apparatus and Procedure

The experimental study of transverse discharges in MHD generators was performed in a copper-shell test section installed in the MHD Simulation Facility. The facility provides plasma conditions similar to those which would be found in full-scale MHD generators. Diagnostic devices both on

the channel and in the facility provide experimental data. A motor-generator set is used to provide a DC potential across the channel allowing a study of the basic physics of current transfer from electrodes to a flowing gas without the complication of a magnetic field. Once the physics of this simplified situation is properly understood, a magnetic field can be added and the more complex phenomena studied.

MHD Simulation Facility

The MHD Simulation Facility of the MIT Energy Laboratory, shown schematically in Figure 1, consists of a water-cooled copper combustor fueled by a mixture of JP-4 aviation fuel and an oxygen and air oxidizer gas mixture^[3]. The ratio of oxygen to nitrogen in the final gas mixture, altered by adjusting the mass flow rates of the oxygen and air, controls the final temperature of the flame. To enhance the gas conductivity, a KOH/H₂O "seed" solution is also injected into the combustor. The test section is mated directly to the exit nozzle of the combustor. The physical properties of the combustion gases are calculated using an equilibrium chemical composition code.^[8]

Channel Design and Construction

Channel Casing

The channel consisted of a copper casing lined with a double layer of MgO firebrick (as shown in Figure 2). The low thermal conductivity of the MgO results in the exposed surface of the brick running hot, providing a hot boundary layer. The hot surface was designed to run at approximately 2200 K by proper selection of brick thickness. The double layer of brick,

whose pattern is shown in Figure 3, was chosen both to inhibit current leakage to the channel case and to allow some thermal expansion of the brick to avoid spalling.

The flange and bricks at the exit of the channel were notched to provide a line of sight along the top of the channel for the line-reversal temperature measurements discussed below.

To insure leak-tightness, the channel components were sealed with RTV (silicone rubber) and tested under an internal pressure of 1.5 psig.

Electrode Modules

Three electrode pairs were used, with the outer two pairs "guarding" the central electrode pair. The electrode modules, each containing three electrodes separated by insulators, were installed in the narrow wall of the channel, approximately 5 inches downstream of the channel entrance. The electrodes were made of 304 SS blocks ($1 \frac{7}{8}$ " x $1 \frac{11}{16}$ " x $\frac{1}{2}$ ") brazed onto copper blocks through which cooling water flowed. The three electrodes were mounted on a G-10 fiberglass backing piece and were electrically insulated from each other by $\frac{1}{16}$ " thick, recrystallized alumina plates. The electrode module assembled and in place is shown in Figure 4.

The near side of the stainless steel cap of each electrode was slotted to increase the thermal resistance to the coolant. The depth of the slots increased with downstream position of the electrode resulting in more uniform surface temperature for all electrodes (axial changes in the heat transfer characteristics resulting from the step wall temperature change and the general boundary-layer thickening would otherwise cause electrodes further downstream to run cooler than those upstream). A non-uniform surface temperature would have introduced further complications and possible systematic errors into the analysis of the data and comparison with theory.

The surface temperature of the electrodes was measured by Cr/Al thermocouples embedded near the hot surface. The electrode slot depth was designed such that the surface temperature was in the range 1000 K to 1200 K. The thermocouples were constructed of 10 mil wires inserted into 1/16" OD double bore (1/64" ID) alumina tubing; the wires were welded together at the sensing end with an oxy-acetylene torch. The thermocouples were held in place in the electrode module with Ceramabond, a ceramic adhesive.

Instrumentation

The experiment was designed to measure the discharge characteristics in the boundary layer and also measure the electrical conductivity in the core of the flow. For this purpose, the channel was equipped with four arrays of voltage probes, whose positions are indicated in Figure 5. The voltage probes were constructed of 20 mil, Pt/Pt-10%Rh wire, encased in 1/16" OD alumina tubing. These probes were inserted slightly into the gas at various levels above the electrodes, through holes drilled through the copper shell and brick liner. As shown in a later section, the voltage probes nearest the electrodes are within the boundary layer while the remainder are in the core region. Eleven voltage measurements were made across the test electrodes (on each side of the channel) and seven across the guard electrodes (on one side of the channel only).

The probe voltages were measured differently in various configurations. The use of differential measurements reduced the possibility of any effect of local fluctuation in gas properties. For example, if all local voltages had been measured with respect to ground, and differential voltages computed numerically, then there would be the possibility of property fluctuations on the short time scales between successive probe measurements affecting the results; the measurements were multiplexed into an A/D con-

verter with a 0.02 delay between successive readings. In several cases, a voltage which was measured by a single differential connection was also measured by a series of smaller differential steps. The comparison of the sum of the smaller steps to the total differential was used as a consistency check; i.e., if the two did not agree this was taken to indicate a failure of some component in the system - e.g., one of the voltage probes.

The probe and electrode voltage data were recorded by a multiplexed 72-channel A/D converter (termed "ICR") and stored on magnetic disk by a PDP 11/60 computer for post-run analysis^[3]. The maximum common-mode level of the ICR was 300 V; however, the maximum differential voltage was only 10 V. Since up to 250V D.C. was applied across the channel, it was necessary to divide the voltage to ground before input to the ICR, as shown in Figure 6a. Since the voltage dividers had a high output impedance (on the order of 2 M Ω) while the ICR had a very low input impedance (about 660 Ω), it was necessary to use the buffer amplifiers indicated in Figure 6a. The current into and out of each electrode was also measured independently by a series resistor, shown schematically in Figure 6b. This was also recorded.

In addition, a pressure tap measured the local pressure 1" above the channel entrance. A line-reversal technique^[4] was used to measure the gas temperature at the channel exit. The surface temperature of the MgO brick was measured by Pt/Pt-10%Rh thermocouples embedded in the brick near the hot surface and the computed temperature gradient gave a measure of the heat loss through the channel walls.

To establish the mode of current transport near the electrodes (i.e., arc vs. diffuse) both videotape and super-8 film were used to record visual observations of the surface of one electrode module and of the plasma near that surface. A special mirror mount was designed and constructed to give

a direct view of the electrodes on one side of the channel and the resulting view was split with a mirror beam-splitter so that images through both blue and red filters could be recorded. The image to the videocamera was filtered through a narrow-band red filter, which screened out nearly all the plasma radiation due to the potassium seed, providing an image of the electrode surface. The super-8 movies were taken through a wide-band blue filter, which allowed both plasma radiation and light due to thermal emission from the wall to pass, but which was more sensitive to the plasma radiation. To keep the main mirror free of dust and seed during the run, air was passed through a diffuser to form a gas curtain across the mirror.

Fuel and oxidant flow rates were also recorded during the run and the combustor was instrumented to provide burner pressure and component temperatures. Resistance Temperature Devices (RTD's) were used to measure the temperature changes of the water coolant to the combustor from inlet to outlet. These last, along with the temperature measurements on the channel itself, allow a total heat balance for the system to be calculated.

Most data obtained during the run were converted to digital form in real time by the ICR system and stored on disk. A flexible data-display program was written and interfaced with the main sampling routines to allow constant monitoring of key parameters during the experiment.

Some of the data, specifically thermocouple data, were recorded on an 8-channel strip-chart recorder and manually from a digital readout, because the common-mode voltage of the thermocouple leads (up to 250 V) was too high for the ICR. Some voltage data were also recorded on a strip chart both to provide a benchmark against the computerized data acquisition programs, and also to check for phenomena which occur too fast for the ICR system to resolve. The ICR could not record fast transients since each

channel input to the ICR was coupled to the A/D converter through an RC with a 1.5 s time constant. In addition, the sampling rate (all channels) was only approx. 50 Hz.

Experimental Procedure

The experiment was performed in two parts - a checkout run followed two days later by the actual experiment. The experimental conditions to be run were specified before the experiment, and detailed instruction sheets were prepared for each of the 11 people assisting with the experiment.

The checkout experiment was structured in four parts: a thermal phase, a conductivity phase, a transition to arc phase, a fluctuation phase and finally a seed burnout phase. The thermal stage consisted of a few short periods (3-5 minutes each) at increasing temperatures. Several key thermocouples were monitored to ascertain the thermal performance of the channel and any irregularities were noted. This was followed by the conductivity stage during which DC voltages were applied across the channel. Subsequent to this, the voltage was raised in an attempt to cause transition from diffuse to arc current transport. To explore the possibility of fluctuations in conditions correlating with voltage fluctuations various probe voltage fluctuations were recorded by oscilloscope simultaneously with the output of the photomultiplier tube used by the line-reversal optical apparatus. Finally, to burn the seed out of the channel, the combustor was run with no seed injection for several minutes. The combustor was then shut down. With the data obtained from this checkout run, the detailed run plan for the main experiment was drawn up based on the experimental data obtained.

The main experiment was somewhat different, consisting of seven parts: a low-temperature thermal phase; two low-temperature conductivity phases; a high-temperature thermal phase; two high-temperature conductivity phases; and the shutdown phase. The low-temperature thermal phase was similar to that of the checkout run, but much briefer, since the low temperature performance of the channel had already been established during the previous run. The electrical tests were more extensive than those of the checkout experiment. For each electrical test, a specific voltage was applied across the channel for 30 seconds, during which time all data acquisition and recording were performed. The tests were done at several different voltages, staggered to avoid systematic errors. After these tests, the electrical leads to the channel were switched and the tests repeated. This procedure allowed the anode to be filmed during the first set of conditions, and the cathode to be filmed during the second set.

The flame temperature was then raised in two steps to avoid thermal shock of the MgO liner, and the thermal performance of the channel monitored. Electrical measurements at this high temperature were then done in a manner similar to the low-temperature tests. Following this, the combustor was shut down.

2b. Experimental Results and Interpretation

The principal experimental measurement was the potential distribution in the vicinity of the electrodes and in the region away from the electrodes (i.e. in the core of the flow) as a function of current through the channel. This measurement, with suitable corrections to account for the three-dimensionality of the current distribution, allows a determination of

the conductivity in the bulk of the flow. From the conductivity measurement the core temperature can be inferred. This value of core temperature is used as a boundary condition for the analysis of boundary-layer voltage drops presented later in this section. The experimental voltage drops are presented here.

Test Conditions

The various test conditions achieved during the main experimental run and the series of applied voltages used during the electrical run are listed in Table 1. To summarize here, N_2/O_2 ratios from 1.5 to 0.5 were run. A seed fraction of 1% potassium by weight in the combustion products was maintained during the electrical phases. The line-reversal technique indicated the temperature at the exit of the channel to be $2600\text{ K} \pm 50\text{ K}$ for N_2/O_2 of 1.0. The mass flow rates of oxidant, fuel and seed as well as combustor heat losses are indicated in Table 2. Key component temperatures during various conditions are also listed; during the low temperature electrical tests, the electrode surface temperature was approximately 1150 K, and the brick surface temperature 2200 K. At the higher-temperature test conditions, the surface temperatures were approximately 1500 K and 2400 K respectively. In fact, melting of the stainless steel was observed during these conditions.

Electrical Measurements

The overall values of MG (motor generator) voltage, channel voltage for each electrode pair, and anode and cathode current for each electrode

pair are shown for representative conditions in Figures 7a and 7b. The anode and cathode currents for each electrode pair are clearly not the same. Due to the fact that the circuits for the electrode pairs are not isolated from each other, and that the plasma has finite axial and spatial nonuniformities, the current entering the electrode pair need not equal the current leaving the pair. However, the sum of all three anode currents does equal the sum of all cathode currents, showing that there is no leakage through the combustor to ground.

Typical distributions for both sides of the channel are also shown in Figures 8a and 8b. To construct the profiles, zero absolute voltage at the anode was assumed and the voltage at each probe constructed by summing the differential voltages of all preceding probes. The measurements in the core of the channel are in good agreement between the two sides, with differences on the order of 10% or less in most cases. Closer to the anodes and cathodes, however, the inconsistencies between the two sides approach 30% - 50%. This may be due to nonuniformities in current densities near the electrode surface, an effect which has been observed in other experiments^[1]. These non-uniformities could also be due to local heating of the electrode surface, which results in a lower boundary-layer resistance locally and thus a higher current density.

The linear profile in the core indicates constant properties there, and can be used to estimate the core conductivity, as calculated in the following section. The effects of the boundary layers and cathode voltage drops are clearly evident in the profiles.

The profiles are consistent between the two sides of the channel except for the high-current conditions for the second set of electrical tests. At this point, profiles obtained for each side are inconsistent.

This was interpreted to indicate the failure of some of the probes. However, the probes on one side of the channel appear to maintain the linear core voltage profile, so there are still usable data here for the succeeding conditions of the #200 series of tests. Consistency tests on the data indicated a good level of reliability (consistent to within 2 - 5%) until the higher currents in the second set of low-temperature measurements. At this point (#212 and up) inconsistencies of 10% and greater are noted, indicating the progressive failure of the probes.

At higher temperatures (#300 - #500), the data indicate failure of many probes and extremely high surface temperatures of the electrodes. Melting of the stainless steel electrode caps was observed. The data from these high-temperature conditions were discarded due to these difficulties.

The load lines for the two probes nearest the central electrodes are plotted for anodes and cathodes for both sets of low-temperature electrical tests. These are shown in Figures 9 and 10. The cathode drop is clearly evident and is on the order of 50 V. On the anode side, the low-current (no Joule heating) slope is well defined. At higher currents (up to about 3 A/cm^2) the effects of Joule heating become evident in the decrease in the slope of the load line.

Visual Observations

The visually-recorded data clearly establish the primary mode of current transfer as diffuse, especially for the anodes. Although some arcing was observed at all voltages above 100 V, for the anodes this was entirely interelectrode in nature and arose due to the fact that not all electrode voltages were turned on simultaneously. Thus, there were

momentary large voltage differentials between adjacent electrodes, leading to the formation of arcs. After all electrodes reached the same voltage the arcs disappeared, leaving only the diffuse discharge. Some arcs were observed on the edges of the cathodes, and these persisted even when all the electrodes reached the same voltage. This is a common cathode phenomenon.

Brightening of the electrode surface was observed at the higher current densities. This might have been due to direct Joule heating of the surface or reflected radiation from Joule heating of the gas near the surface. The brightness was not uniform over the surface. One side of the electrode surface was consistently brighter than the other.

The main mirror stayed relatively clean until the higher temperatures were obtained. Most of the obstruction on the mirror during the low-temperature conditions was caused by the initial start of the combustor.

Thermal Performance of Channel

The electrodes operated at higher than design temperatures. This was primarily due to combustor mass flow rates which were 25% higher than anticipated. Also, in the initial heat-transfer calculations for the design of the electrodes, net radiation from the hot brick walls and plasma, which amount to about $0.1 - 0.2 \text{ MW/cm}^2$ ^[5], was neglected. When these effects are taken into account, the thermal performance of the channel is quantitatively understandable.

Due to the overheating of the channel, at conditions of $N_2/O_2 = 0.5(T_{\text{plasma}} = 2700 \text{ K} \pm 50 \text{ K})$ the electrode caps and the interelectrode

insulators fused and began to flow along the brick liner. This made further measurements unreliable.

Calculation of Core Conductivity and Core Temperature

Certain assumptions were made about the spatial variation of the plasma conductivity and an estimate of the core conductivity was obtained from the total current and the voltage measurements in the core region. This estimate of core conductivity was used to infer the core temperature which was then used in the more sophisticated analysis presented in succeeding sections.

If careful measurements could be made directly in the core region, the relation

$$J = \sigma E$$

(where scalar conductivity σ is used since there is no magnetic field in this experiment) could be used to directly measure the core conductivity. However, voltage probes cannot be inserted directly into the core region because they would disturb the flow and the measurements, and also because they would most probably not survive the harsh environment. Furthermore, there is no easy way to measure local current density. Thus, the experiment is limited to measuring the total current through the plasma and the voltage at the side walls, as shown in Figure 11a.

The problem of determining the core conductivity from the measurements made is complicated by the fact that σ , $\bar{J}$, and $\bar{E}$ are all spatially dependent. The primary difficulties, which are shown schematically in Figure 11b, are:

- (1) The effects of the boundary layers on the y and z walls.
- (2) Current spreading in the axial (x) direction which is further complicated by:

(3) Axial property variations.

To account for these effects a model was assumed for the spatial variation of σ . Applying the model to the measured quantities gave an estimate for the core conductivity σ_c .

To facilitate modeling the spatial variations of σ , the variations were assumed to have the form

$$\sigma(x,y,z) = \sigma_c \sigma_x(x) \sigma_y(y) \sigma_z(z) \quad (2)$$

where σ_x , σ_y , and σ_z are dimensionless. The conductivity is assumed to be uniform and equal to σ_c in the core region, so σ_x , σ_y , σ_z are unity for any x,y,z in the core.

The heat loss through the channel case was not great and the flow area was constant. The boundary layer thickness is given by

$$\delta = 0.370 x/R^{*0.2} \quad (3)$$

where

$$R^* = \rho^* u x / \mu^* \quad (4)$$

is the Reynolds number based on axial position with the properties calculated at a reference state^[6]. For moderate velocity, and Prandtl numbers near unity, the reference state is evaluated approximately at the local pressure and mean static enthalpy across the boundary layer; that is

$$h^* = (h_{core} + h_{wall})/2 \quad (5)$$

The boundary-layer thickness at the level of the voltage probes was only about 5 mm (as compared to the 1.625" x 3" dimensions of the channel cross-section) and grew only as the $x^{0.8}$. Thus, the axial variation of σ , which affects only the spreading of current in x slightly, was neglected. That is

$$\sigma(x,y,z) = \sigma_c \sigma_y \sigma_z \quad (6)$$

With the axial variation in σ assumed small, the current-spreading

effect in the axial direction can be approximately accounted for. To make this correction, a two-dimensional cross-section (i.e., $z = \text{constant}$) model of the plasma/electrode configuration was constructed with Teledeltos paper. Teledeltos paper has a finite surface resistivity and can be used to construct electrical models of systems which obey a two-dimensional Laplace's equation. Assuming uniform conductivity, the spreading of current in the axial direction is accounted for by a geometric factor g applied to the current density; i.e., the current density is taken as

$$J' = gI/A$$

where I/a is the current density at the electrodes and A is the factor determined from the teledeltos paper solution. For the dimensions of the channel, $g = 0.54$. Two factors make this correction only approximate. First, downstream of the exit of the channel, cold air mixes with the hot gas, lowering the conductivity, and invalidating the assumption of small axial variation in conductivity. The large size of the combustor also introduced error. However, these end effects become negligible if the total length of the uniformly-conducting gas is 6 to 8 times the distance between electrodes. To test this, one inch was cut from each end of the paper, and it was found that the newly resulting value of g differed by only about 1% from the previous value. The model is also approximate because it does not account for the y variation due to the boundary layer, which affects the spreading of the current. However, since the total boundary-layer thickness was small compared to the height of the channel (8 cm vs. 0.5 cm) this effect was ignored.

The next step in inferring the core conductivity involves correction for the y and z variations. First, since the core voltage probes are at specific y locations outside the spatial extent of the y variation, the

y-variation can be neglected. The remaining relation is

$$\bar{J} = \sigma_c \sigma_z(z) \bar{E}$$

Since J_z and J_x are zero in the core, only the y component must be considered so,

$$J_y = \sigma_c \sigma_z(z) E_y \quad (11)$$

In the core region the z-variation in σ can be thought of as a series of parallel conductances through the plasma. The z-variation accounted for by averaging equation 11

$$\int_{-w/2}^{w/2} J_y dz = \sigma_c \left\{ \int_{-w/2}^{w/2} \sigma_z(z) dz \right\} E_y \quad (12)$$

with E_y assumed independent of z (since $\text{curl } E = 0 = E_z$, this implies $\partial E_y / \partial z = 0$). This ignores current spreading in x. To take this into account, the spreading factor g in the core is applied to yield

$$gI/A = E_y \langle \sigma \rangle_z = E_y \sigma_c \langle \sigma_z \rangle_z \quad (13)$$

where A is the area of the central electrode (7.2 cm^2) and $\langle \sigma \rangle_z$ is the conductivity averaged over z. Thus, to derive a value for σ_c , the value of $\langle \sigma_z \rangle_z$ must be calculated and I and E_y measured. The current, I, is measured directly and E_y may be estimated using the voltage differentials, ΔV , and the distances between probes, d; i.e.,

$$E_y \sim \Delta V/d \quad (14)$$

To calculate $\langle \sigma_z \rangle_z$, the model profile used by Kessler^[7] was assumed. This was a 1/7th power law profile for the total enthalpy, $h_t = h + u^2/2$, given by

$$\begin{aligned} (h_T - h_{TW}) / (h_{TC} - h_{TW}) &= (z/\delta)^{1/7} \quad (z < \delta) \\ h_T &= h_{TC} \quad (z > \delta) \end{aligned} \quad (15)$$

where σ is the boundary-layer thickness and the subscripts c and w refer to core and wall values respectively. The pressure p is known and assumed

constant, and the axial velocity u is assumed to be given by a 1/7th power law profile

$$\begin{aligned} u/u_c &= (z/\delta)^{1/7} & z < \delta \\ u &= u_c & z > \delta \end{aligned} \quad (16)$$

where u_c is the core velocity. With h_T , u , and p known as functions of z and with the assumption of equilibrium properties, $\sigma = \sigma^*(h_T(y), p(y))$ was calculated and used to evaluate $\langle \sigma \rangle_z$.

The wall quantities were known since the surface temperature of the brick was measured. Thus, the only unknown independent quantity was h_{TC} . h_{TC} was varied until $\langle \sigma \rangle_z$ agreed with the experimental values and the core conductivity estimated as $\sigma_{(z=0)}$. The core temperature, T_c , was determined from h_{TC} and u_c .

The value of current used was the average between anode and cathode

$$I = (I_{\text{anode}} + I_{\text{cathode}})/2 \quad (17)$$

and ΔV was estimated by averaging the four central ΔV measurements on both sidewalls.

An estimate of σ_c was obtained for each condition and an average taken for the #100 runs and the #200 runs. The final results are shown in Table 3. The inferred core temperature is well within the line-reversal measurement of $2600 \text{ K} \pm 50 \text{ K}$. The estimated core values were used as boundary conditions for the theoretical analysis presented in succeeding sections.

The core temperature at the axial location of the electrodes was also calculated by using the Chemical Equilibrium Code [8]. The measured fuel and oxidizer flow rates and combustor heat losses were input to the code, and the resulting flame temperature calculated. The hot gas core was then considered to flow adiabatically to the location of the electrodes. The core temperature at this point was calculated to be approximately 2530 K, in fair agreement with the inferred values of core temperature.

2.2c Theoretical Analysis of Near-Electrode Current Transport

This section presents a general discussion of the calculation procedures used to evaluate the electron number density in the boundary layer of the channel. A simple discharge configuration is treated to display the physics of the problem and only the outer or charge neutral region is discussed. This discussion draws primarily upon the work of Unkel [1]. The primary computational tool is a numerical boundary layer code developed by Daily [9] whose results are then used to help formulate and evaluate simple models of the electron density profile, such as that by Kessler and Eustis [7].

Introduction

For the purpose of illustrating the important features of the calculation of the number density profile, consider a one-dimensional discharge through the boundary layer formed over an electrode wall, as shown schematically in Figure 12. For this discussion a typical clean combustion products flow is assumed and the electrode is assumed to be metallic and operating at the same surface temperature as the upstream insulating wall. These conditions are chosen to represent the experimental conditions discussed in the test, and are not fundamental restrictions imposed by the applicability of the theory. Current densities from zero to 3 A/cm^2 are considered for anodes only, since the complex surface effects at the cathode are beyond the applicability of the theory. Limitations and extensions of the theory in terms of violation of charge neutrality, charge transfer by diffusion and electron temperature non-equilibrium are discussed.

Model for the Outer Region

For the conditions stated, the majority of the gasdynamic boundary layer region is characterized by low electric field gradients and low thermal energy per particle. Departure from charge neutrality can thus be considered small and the electron and ion number densities taken to be equal. The species conservation equations for electrons and ions can be combined, assuming $n_e = n_i$ to form the ambipolar diffusion equation and a current conservation equation. The ambipolar diffusion equation for the mean electron concentration in the turbulent flow field is

$$u \frac{\partial C_e}{\partial x} + v \frac{\partial C_e}{\partial y} = \frac{1}{\rho} \frac{\partial}{\partial y} (\rho D_a + \rho \epsilon_E) \frac{\partial C_e}{\partial y} + \frac{m_e \dot{n}_e}{\rho} \quad (18a)$$

with the net ionization rate of Curry [14] given as

$$\dot{n}_e = 2.52 \times 10^{-33} n_e \exp(-2.98 \times 10^{-3} T) (n_e^{*2} - n_e^2) \quad (18b)$$

where $C_e = n_e m_e / \rho$ is the electron concentration, D_a and ϵ_E are the ambipolar diffusion rates for molecular and turbulent transport,

respectively and $\dot{n}_e$ is the net ionization rate. With the electrons and heavy gas maintained close to equilibrium the electron energy equation need not be solved, as the heavy particle continuity, momentum, energy and state equations, together with the electron continuity equation, determine the electron (and ion) number density profiles in the charge-neutral region.

For this illustrative calculation, only the transverse current density, J_y , will be non-zero and it will be assumed constant along the electrode length and zero elsewhere. For this case, the current conservation equation is

$$J_y = (\mu_e + \mu_i) en_e [E_y + \frac{1}{n_e} \frac{d}{dy}(n_e kT_e)] \quad (19)$$

where μ_e and μ_i are the electron and ion mobilities, respectively. In the

limit of $\mu_e \gg \mu_i$, $E_y \gg \frac{1}{n_e} \left(\frac{d(n_e kT_e)}{dy} \right)$ Equation (19) reduces to

$$J_y = \sigma_e E_y \quad (20)$$

where $\sigma_e = n_e \mu_e e$ is the electron conductivity. The simple Ohm's law represented by Equation (20) indicates that current transfer is accomplished predominately by the more mobile electrons. The electron and ion number profiles are controlled predominately by the motion of ions, as

$$D_a = \frac{2kT}{e} \frac{\mu_e \mu_i}{\mu_e + \mu_i} \approx 2D_i \quad (21)$$

where D_i is the ion diffusion coefficient, given by

$$D_i = 0.00369 kT \mu_e / e \quad (22)$$

The Equations (18) through (22) are combined with the standard boundary layer equations and a mixing length turbulence model to yield the set of equations presented in Figure 13.

Limitations of the Model for the Outer Region

The model presented in the previous section incorporates several assumptions and cannot be used to represent the behavior over the entire boundary layer. In particular, large electric field gradients occur near the electrode wall, resulting in a violation of charge neutrality. Unkel [1], using a criterion suggested by Self [10], has shown that significant violations of charge neutrality occur for regions closer to the wall than about 10 to 50 microns. Thus, the model presented in the previous section

cannot be used to calculate the electron number density any closer to the wall than about 10^{-5} m. A detailed discussion of the characteristics of this inner, or sheath, region, is contained in Ref. 1; however, for the purpose of this discussion, the sheath is ignored and only the outer region is considered.

To derive Equation (20) the assumption that

$$E_y \gg \frac{1}{n_e} \left(\frac{d(n_e kT_e)}{dy} \right) \quad (23)$$

was made. This is certainly not true for no applied current i.e. $J_y = 0$. Thus, the relative importance of charge transfer by electric field and by diffusion must be considered, before the approximation indicated by Equation (23) can be made. It can be shown [1] that for current densities of interest (greater than $.5 \text{ A/cm}^2$) charge transfer by electric field dominates the diffusion process in the outer region. Thus, Equation (20) can be applied and current conservation need not be considered.

In the core region and throughout most of the boundary layer, inelastic collision processes in a combustion products plasma equalize electron and heavy particle temperatures. Closer to the wall, however, the electron number density is low, and for high enough current density, Joule dissipation in the electron gas may be sufficiently great to result in significant electron temperature elevation.

A simple model for this effect can be derived if collisional loss to the heavy species is considered the dominant energy loss mechanism for the electrons. Balancing the rate at which energy is imparted to the electrons by the electric field and the collisional loss rate, the electron temperature can be estimated from

$$\frac{3}{2} k (T_e - T_h) \approx \delta^* \frac{m_e}{m_h} \bar{\nu}_{eh} = \frac{1}{n_e} \frac{J^2}{\sigma_e} \quad (24)$$

where $\bar{\nu}_{eh}$ is the electron-heavy particle collision frequency, T_e and T_h are the electron and heavy particle temperatures, respectively and the quantity $(2\delta^* m_e/m_h)$ represents the average fraction of energy loss per collision. The physics of the collisional process is contained in the evaluation of δ^* , termed the energy loss factor (ELF). For a noble gas plasma, where elastic collisions are dominant, $\delta^* \approx 1$. However, $\delta^* \approx 2000$ is typical for a combustion gas plasma [1]. This electron heating model is included in the boundary layer analysis, and its effects can be altered by adjusting δ^* . For example, letting $\delta^* \rightarrow \infty$, will cause $T_e \rightarrow T_h$, and thus the effects of electron heating can be turned on or off to study the resulting electron density profiles.

Increased electron temperature will affect the ionization-recombination rates, the electron pressure gradient term in the electron continuity equation, the electron mobility and the molecular ambipolar diffusion coefficient. The altered reaction rates are taken into account by evaluating the reaction rate formulae at the electron temperature rather than at the heavy particle temperature. The electron pressure gradient term, which contains an explicit dependence on electron temperature is relatively unimportant in the outer region and the increased electron temperature will tend to further decrease its importance, since as n_e goes down near the wall, T_e goes up, so the net effect will be a smaller pressure gradient. That is

$$\frac{d(n_e k T_e)}{dy} < \frac{d(n_e k T_h)}{dy} \quad (25)$$

Thus, this effect is ignored in the outer region and Equation (20) is maintained.

Since the electron mobility depends on both the electron and heavy gas temperatures, this should be taken into account in the calculation of electron density profiles. However, no simple model of this dependence was available, so both D_a and μ_e were treated as functions only of T_h .

3d, Numerical Solution of Boundary Layer Equations

The boundary layer equations presented in Figure 13 were solved numerically by a code originally developed by Daily [9] and modified for the purposes of the present investigation. The modifications consisted of the addition of the code of Equation (24) to allow electron temperature non-equilibrium, and the addition of physical properties subroutines which provide the properties of the JP-4 combustion products as a function of enthalpy and pressure.

Among other things, the code provides the electron number density (n_e), enthalpy (h), axial velocity (u), heavy particle temperature (T_h), and electron temperature (T_e), as functions of axial (x) and transverse (y) position in the flow.

Modeling of Electron Number Density Profile

Approximate methods of computing electron number density profiles and the resistance through the charge-neutral region requiring minimal calculation time would be useful for MHD generator performance models and other situations where the detailed response is not overly important. Using the electron number density profiles generated by the boundary layer code as a benchmark, several approximate methods of computing n_e are examined and their usefulness and restrictions discussed. A model suggested by Unkel [1]

divides the boundary layer into an "equilibrium" region, where

$$n_e = n_e^* = n_e^*(h,p) \quad (26)$$

and a second "frozen" region where the electron reaction rates are small compared to diffusion and convection effects and which can be modeled by profiles similar to the standard two and three-layer profiles used to model the static enthalpy. This model has been further evaluated in the present work.

Power Law Profile for Non-Dimensional Total Enthalpy

The simplest and most commonly used method is to assume a power law profile for the non-dimensional total enthalpy and to evaluate the electron number density from the resulting temperature profile. That is, assuming

$$\left(\frac{h_T - h_{TW}}{h_{TC} - h_{TW}} \right) = \left(\frac{y}{\delta} \right)^P \quad y < \delta \quad (27)$$

$$h_T = h_{TC} \quad y \geq \delta$$

and with n_e calculated as

$$n_e = n_e^*[T(h(y)),p] \quad (28)$$

This is the method used by Kessler [7] to compute boundary layer resistance with $P = 1/7$. The agreement Kessler obtained with theory was good; however, the agreement can be considered to be fortuitous, the recombination rates presented are correct.

Figure 14 compares $n_e(y)$ obtained by using Equations (27) and (28) to the profiles generated by the boundary layer code. The agreement of the electron density profile is fairly good, which explains Kessler's results and why the profile of Equation (25) is used so frequently. However, as Figure 15 shows, the predicted values of static enthalpy based on

Equation (27) are in disagreement with the numerically calculated profile. Clearly, important physics has been left out by using the 1/7th power law profile. Insofar as modeling the enthalpy profile, the power law profile can be improved by the addition of a laminar sublayer.

Total Enthalpy Profile with Laminar Sublayer

A total enthalpy profile which more closely agrees with experiment, especially close to the wall, is given by

$$\begin{aligned} h_T &= h_{TC} & y > \delta \\ \left(\frac{h_T - h_{TW}}{h_{TC} - h_{TW}} \right) &= \left(\frac{y}{\delta} \right)^{1/7} & \delta > y > y_L \\ \left(\frac{h_T - h_{TW}}{h_{TC} - h_{TW}} \right) &= \left(\frac{y}{y_L} \right) & y < y_L \end{aligned} \quad (29)$$

where y_L , the thickness of the laminar sublayer, is given empirically by [9]

$$y_L = \frac{30}{0.151} \left(\frac{1}{\delta} \right)^{-0.125} \left(\frac{\mu_c}{\rho_c u_c} \right)^{0.875} \quad (30)$$

and h_{Tsub} , the total enthalpy at the edge of the sublayer is given by

$$h_{Tsub} = h_{TW} + (h_{TC} - h_{TW}) (y_L/\delta)^{1/7} \quad (31)$$

Figure 15 indicates better agreement for this profile with numerical calculations than the profile of the previous section; Figure 14 demonstrates that the equilibrium electron number density matches, roughly, the equilibrium density calculated by the boundary layer code. However, the agreement with the kinetic profile is poor, since the kinetic and equilibrium electron number density profiles near the wall are quite different. The analysis must depart from equilibrium consideration to a direct analysis of the kinetic profile.

Detailed Consideration of Calculated Electron Density Profile

From a consideration of Figure 14, it is clear that the electron density profile cannot be reasonably approximated by the equilibrium profile, for the recombination rates assumed. The equilibrium profile may be considered to be the limiting case of the electron continuity equation for high reaction rates; thus, it may be constructive to consider the limiting case of negligible electron-ion reaction rates in the boundary layer. In this case, Equation (18) becomes

$$u \frac{\partial c_e}{\partial x} + v \frac{\partial c_e}{\partial y} = \frac{1}{\rho} \frac{\partial}{\partial y} (\rho D_a + \rho \epsilon_E) \frac{\partial c_e}{\partial y} \quad y < \delta \quad (32)$$

$$= \frac{1}{\rho} \frac{\partial}{\partial y} (\rho D_a + \rho \epsilon_E) \frac{\partial \psi_e}{\partial y} + \frac{m_e \dot{n}_e}{\rho} \quad y > \delta \quad (32)$$

This case is also shown in Figure 16 where the reaction rate constant has been set to zero in the boundary layer, and equal to its normal value in the core. As indicated in Figure 16, this new profile closely resembles the true calculated profile in shape although it lacks quantitative agreement. However, if the point at which the reaction rate is set equal to zero were chosen closer to the wall, this profile could be made to approach the calculated profile more closely.

The profile resulting from Equation (32) is determined by a balance of convection and diffusion. The resulting "frozen" (i.e. $\dot{n}_e = 0$) profile would have the same character as the axial velocity or total enthalpy profiles. In fact, for turbulent and molecular Prandtl and Lewis numbers of unity, the non-dimensional profiles of electron density, axial velocity and total enthalpy would be the same, as an examination of the axial momentum con-

tinuity and energy equations show. The scaled axial velocity profile plotted in Figure 16 clearly shows this behavior at distances closer to the wall than 1×10^{-4} m. It appears that the following is true: in the core, the reaction rate is high enough to maintain an equilibrium number density - diffusion and convection are unimportant. Then, passing through the boundary, these reactions become less dominant and the profile deviates from equilibrium. There is a transition region in which diffusion, convection and reactions are all important and in which the number density profile lies between the fully frozen and equilibrium profiles. Finally, yet closer to the wall the profile freezes and presumably follows a frozen profile to the sheath region. For this case, the profile appears to freeze near the edge of the laminar sublayer, which is at about 6×10^{-5} m from the wall ($y^+ = 5$).

This discussion suggests a procedure for developing a simple model for the electron density profile near the wall. The density profile closely follows the axial velocity profile in the region where the density profile is frozen. Axial velocity profiles have been successfully modeled by two-layer and three-layer models [12] which, by the reasoning given in the previous discussion, could also be used to model the frozen electron density profile.

It is clear from Figure 16 where the approximation of frozen profile is valid. An approximate method of constructing the profile would be to assume an equilibrium profile from the core to some point in the boundary layer and a frozen profile from that point (the "freeze point") to the wall. This procedure neglects the complicated transition region but was found to produce rough agreement with the calculated electron density profile.

The freeze point was taken to be the point at which the true profile departed from the equilibrium profile by a given amount, the departure being defined as

$$FR = - \frac{n_e^{*2} - n_e^2}{n_e^{*2}} \quad (33)$$

Using Equation (32), FR can be rewritten as

$$FR = \frac{\rho u \frac{\partial c_e}{\partial x} + \rho v \frac{\partial c_e}{\partial y} - \frac{\partial}{\partial y} [\rho D_a + \rho \epsilon_E] \frac{\partial c_e}{\partial y}}{\alpha m_e n_e^{*2}} \quad (34)$$

where

$$\alpha = 2.52 \times 10^{-33} \exp ([-2.98 E - 3]T) n_e \quad (35)$$

and

$$c_e = \frac{m_e n_e}{\rho} \quad (36)$$

For small departures from equilibrium, $c_e \approx c_e^*$ and the right hand side of Equation (34) can be evaluated at $c_e = c_e^*$. This will work only so long as c_e is close to c_e^* . In this case

$$FR \approx \frac{\rho u \frac{\partial c_e^*}{\partial x} + \rho v \frac{\partial c_e^*}{\partial y} - \frac{\partial}{\partial y} (\rho D_a + \rho \epsilon_E) \frac{\partial c_e^*}{\partial y}}{\alpha^* n_e^{*2} m_e} \quad (37)$$

Now, n_e^* is a known function of h and p . However, p is constant across the boundary layer so that

$$n_e^* = n_e^*(h) \quad (38)$$

and thus, the derivatives with respect of x and y may be written as

$$\frac{\partial n_e^*}{\partial x} = \frac{dn_e^*}{dh} \frac{\partial h}{\partial x}, \quad \frac{\partial n_e^*}{\partial y} = \frac{dn_e^*}{dh} \frac{\partial h}{\partial y} \quad (39)$$

Thus, making the assumption that ρ varies slowly and using (39), Equation (37) can be written as

$$FR = \frac{\frac{dn_e^*}{dh} [u \frac{\partial h}{\partial x} + v \frac{\partial h}{\partial y}] - \frac{\partial}{\partial y} [(D_a + \epsilon_E) \frac{dn_e^*}{dh} \frac{\partial h}{\partial y}]}{\alpha^* n_e^{*2} m_e} \quad (40)$$

Now, using the energy equation with no Joule heating and with $p = \text{constant}$

$$\lambda_m = \lambda_H \text{ (i.e. } \epsilon_H = \epsilon_m \text{)}$$

$$u \frac{\delta h}{\delta y} + v \frac{\delta h}{\delta y} = \frac{\delta}{\delta y} \left(\frac{k}{\rho c_p} + \epsilon_H \right) \frac{\delta h}{\delta y} + \left(\frac{u}{\rho} + \epsilon_H \right) \left(\frac{\delta u}{\delta y} \right)^2 + 2\mu \epsilon_H \frac{\delta^2 u}{\delta y^2} \quad (41)$$

In the outer part of the boundary layer, h and u may be represented by (1/7)th power law profiles and it can be shown with this assumption that only the first term in (41) is significant. Combining this with (40)

$$FR = \frac{\frac{dn_e^*}{dh} \left(\frac{\delta}{\delta y} \left(\frac{k}{\rho c_p} + \epsilon_H \right) \frac{\delta h}{\delta y} \right) - \frac{\delta}{\delta y} (D_a + \epsilon_E) \frac{dn_e^*}{dh} \frac{\delta h}{\delta y}}{\alpha^* n_e^{*2} m_e} \quad (42)$$

Now, n_e^* varies approximately as

$$n_e^* (h) = n_e^* (h_c) \exp [b (h - h_c)] \quad (43)$$

where b is constant, so

$$\frac{dn_e^*}{dh} = b n_e^* \quad (44)$$

and Equation (42) can be expressed as

$$FR = \frac{b n_e^* \left[\frac{\delta}{\delta y} \left(\frac{k}{\rho c_p} + \epsilon_H \right) \frac{\delta h}{\delta y} \right] - \frac{\delta}{\delta y} b n_e^* (D_a + \epsilon_E) \frac{\delta h}{\delta y}}{\alpha^* n_e^{*2} m_e} \quad (45)$$

From Kays [9]

$$\dot{q}'' = (D_a + \epsilon_E) \frac{\delta h}{\delta y} \rho_c u_c (h_w - h_c) \frac{f_x}{2} \left[1 - \left(\frac{y}{\delta} \right)^{9/7} \right] \quad (46)$$

where

$$\frac{f_x}{2} = 0.0295 R_{ex}^{-0.2}$$

the variation of $k/\rho c_p$ and ϵ_H with y can be estimated assuming velocity and enthalpy profiles

$$u/u_c = (y/\delta)^{1/7}$$

$$\left(\frac{h_T - h_{Tw}}{h_{Tc} - h_{Tw}} \right) = \left(\frac{y}{\delta} \right)^{1/7} \quad (47)$$

Using (47) and (46), Equation (45) can be evaluated as a function of distance from the wall. This approximate calculation is compared with values of FR computed from the boundary layer code in Figure 17.

To pick the point at which the electron density profile is considered frozen, a value for the departure of n_e from equilibrium is chosen and the value of y for which FR gives that departure is taken to be the transition point from equilibrium to frozen profiles.

The model frozen profile was chosen as a (1/7)th power profile to a laminar sublayer and from there a linear profile to the wall. The thickness of the electron density boundary layer was taken to be the same as the thermal boundary layer and the sublayer thickness taken to be at $y^+ = 30$. That is,

$$\left(\frac{n_e - n_{ew}^*}{n_{eo} - n_{ew}^*} \right) = \left(\frac{y}{\delta} \right)^{1/7} \quad y_L < y < \delta \quad (48)$$

$$\left(\frac{n_e - n_{ew}^*}{n_{esub} - n_{ew}^*} \right) = \left(\frac{y}{y_L} \right) \quad y < y_L$$

where y_o is the equilibrium - frozen cut point and y_L is the edge of the laminar sublayer, defined by Equation (30) and where

$$n_{eo} = n_e^* (h_o)$$

$$h_o = h(y_o) \quad (49)$$

$$n_{esub} = n_{ew} + (n_{eo} - n_{ew}) (y_L/\delta)^{1/7}$$

Two such profiles, for FR = 0.05 and 0.80, are compared to the calculated profile in Figure 18. There is qualitative agreement, but no detailed

quantitative agreement, and further work is required if this approach is to produce reasonable results.

A possible improvement would be to use a three-layer profile for the electron density similar to the three-layer velocity profile given by Martinelli [12]. However, the physics of this profile is not well enough understood to make this any better than an empirical fit, so that approach was not used. However, from the output generated by the boundary layer code, it

was noted that the electron continuity source term, that is $m_e \dot{n}_e / \rho$, remained constant to within a factor of three in a large portion of the boundary layer. Thus, a next possible step after modeling the electron density with a zero source term would be to model the electron density using a constant source term. If the convective derivatives in Equation (18) were assumed small and discarded, an ordinary differential equation would result. With appropriate boundary conditions, this could be solved, and the results compared with the calculated profile. Note that the comments above are made based on only 3-body electron recombination being important; as discussed in the next section, 3-body heavy recombination may be important and would somewhat alter the quantitative character of the previous comments.

2d. Comparison of Theory with Experiment and Conclusions

The goal of this work is to evaluate the accuracy of boundary layer resistance calculation by comparison with experimental data. The experiment provides voltage-current profiles in the boundary layer for comparison with voltage-current profiles constructed from the numerical integration of the boundary layer equations believed to represent the physics of the outer

region of the boundary layer (i.e. ignoring sheath effects). Only the anode voltage-current profiles are considered due to the complexity and poor understanding of surface effects at the cathode. To produce quantitative agreement between theory and experiment the electron-ion reaction rates were adjusted to model the effects of three-body reactions (where the third body is a heavy particle) until agreement was obtained. The effects of Joule heating were in all cases underpredicted, even when the adjusted reaction rates were taken into account. Other effects which were not taken into account are mentioned, such as electron temperature non-equilibrium.

Experimental Load Lines in the Boundary Layer

The experimental anode load lines observed for voltage probes in and near the boundary layer are summarized in Figures 19a and 19b. The data for the run numbers 100 and numbers 200 are shown separately for the surface and core conditions determined in section 2b for these two cases. The configuration of the voltage probes is indicated in Table 4.

Theoretical Results

The results of the boundary layer code are shown for comparison in Figure 19a. The electron loss factor (ELF) was made large (2×10^6) to induce electron temperature equilibrium; the effects of electron temperature non-equilibrium are discussed below. The predicted conductance is too high for the probe nearest the wall. However, the voltage drop between the two probes is fairly well predicted, indicating that the electron density profile can be fairly well modeled in the region of the boundary layer close to the core, but that the physics of the region closer to the wall is not yet quantitatively understood.

Since the predicted conductivity is too high, one possibility is that the electron density is not quite as far from equilibrium as the numerical results suggest. Mori [13] has suggested that three-body recombination, where the third body is a heavy particle, may be an important effect. The reaction rate of Equation (18a) includes only three-body recombination, where the third body is an electron. The inclusion of the heavy third particle would increase the reaction rate and thus make n_e closer to n_e^* . To model this effect simply, in terms of avoiding major modifications to the boundary layer code, the reaction rates given by Equation (18a) were allowed to vary by a constant multiplicative factor, A_m ; that is

$$\dot{n}_e = A_m \alpha (n_e^{*2} - n_e^2) \quad (50)$$

where α is given by Equation (35) and A_m is a free parameter input to the boundary layer code. The parameter A_m was allowed to vary until the experimental resistance from the wall to the nearest probe was matched by the code. This value of A_m was found to be equal to 35 for the numbers 100 conditions and the results with $A_m = 35$ are compared in Figure 20 to the experimental results. Allowing A_m to vary, the agreement between theory and experiment can be made quite good at low currents, but Joule heating effects are still underpredicted.

Since the overall conditions for the numbers 200 conditions were quite similar to those for the numbers 100, it would be expected that $A_m = 35$ would produce as good agreement for the numbers 200 as for the numbers 100. However, this was not the case, as indicated in Figure 20b. Although the agreement is good for the probe nearest the wall, the other calculations are in disagreement with the experimental data. This disagreement is believed to be due to a systematic error, since the load line for the probe nearest the wall for the numbers 200 agrees with the numbers 100 while the other load lines do not.

As stated in section 2c, the boundary layer code considers only the outer region of the boundary layer, that is, the regions where charge neutrality can be assumed to hold. However, the electron concentration profile far from the wall can be shown to be insensitive to the values of the electron concentration at the wall, assuming that this is small compared to the free stream value. To show this, consider the non-dimensional profile shape, which can be represented as

$$\left(\frac{c_e - c_{e,wall}}{c_{e,core} - c_{e,wall}} \right) = \frac{c_e}{c_{e,core}} \left(\frac{1 - (c_{e,wall}/c_e)}{1 - (c_{e,wall}/c_{e,core})} \right) \quad (51)$$

For $c_{e,wall}/c_{e,core} \rightarrow 0$, Equation (51) becomes

$$\frac{c_e}{c_{e,core}} \left(1 - \frac{c_{e,wall}}{c_e} \right) = f(y)$$

indicating that the level of $c_{e,wall}$ will be important only in the region very close to the wall, where $c_{e,wall}/c_e$ is not small. Thus, there is some justification for ignoring the sheath region in this analysis.

Other areas which were not included in the analysis were surface effects, and non-uniformities in current densities at the electrode surface which may be due to surface effects. Additionally electron temperature elevation has been ignored. The resistance of a surface layer of material may change markedly with temperature to Joule heating and this may be responsible for the underprediction of Joule heating effects.

The surface of the electrode did visibly brighten during the experiment when the current was on, as shown by the videotape made during the run, so there may have been considerable heating of any surface layer present.

Non-uniformities in current density at the electrode surface might also account for some of the discrepancy between theory and experiment. There were obvious non-uniformities during current turn-on and turn-off in the form of interelectrode arcing. Also, certain areas of the electrode surface were visibly brighter than others, which may indicate these areas carried more current.

2e. Summary of Work Completed

In the first reporting period experimental data were obtained for current transport to electrodes under simulated MHD conditions. Cathode arcs were observed for moderate current densities, however no transverse arcing was observed on anodes at current densities up to 2 A/cm^2 . The simple discharge configuration allowed direct comparison of predicted and measured anode voltage drops, however, predictions were at considerable variance with the measured results. In particular, for lower current densities, where Joule-heating effects should be small, predicted voltage drops were at considerable variance with the measured results. Incorrect ionization-recombination rates were considered as one source of the discrepancy and it was determined that a considerable increase in the rates (electron three-body only) was required to match the low current resistivity, and that when adjusted in this way the effects of Joule heating were underpredicted. Evidence of the importance of three-body heavy particle interactions is strong and these reactions will be included in the future. The calculations indicate also the possibility of significant electron thermal non-equilibrium which could be responsible in part for the strong Joule-heating effects observed. Other contributors to the strong effects of current are electrode heating, surface coatings, and other surface effects.

Attempts to produce transverse arcs at the anode were unsuccessful although current densities of $2 - 3 \text{ A/cm}^2$ were achieved and spatially non-uniform heating of the anode surface was observed. Cathode arcs were observed for many of the experimental conditions. Higher current densities can be obtained in subsequent experiments.

2f. Planned Work and Work in Progress

Experimental

At present the channel is being reassembled for a second experiment and should be ready by mid-July. The channel configuration is shown schematically in Figure 22 and includes three active electrode pairs and voltage probes adjacent to each of the electrodes. To highlight the features of current transport, different upstream wall temperatures will be produced on the two sides and different electrode surface materials will be tested. The different upstream wall temperatures will affect primarily the region of the boundary layer adjacent to the wall, and, assuming that the electron number density is maintained close to the equilibrium, should result in a significant change in resistivity. The different surface materials should result in different surface/plasma interaction characteristics; the materials to be used are copper, stainless steel and platinum. As in the previous test, voltage probes and visual observations will provide the major experimental data. Conditions will be chosen to accentuate arc formation on the anode wall.

Analytical

The initial comparison of theory and experiment suggest several modifications to the boundary-layer code and these will be effected in the next reporting period. Most notably:

- a) Three-body heavy gas recombinations will be included in the reaction rate calculations.

- b) A simplified form of the electron energy equation will be used to determine the degree of electron thermal non-equilibrium and the electron temperature used in calculation of the appropriate rates and transport properties.
- c) Calculation of low current resistivity will include the diffusion terms.
- d) Changes in electrode surface temperature will be included.

Some of the necessary support work for these additions is discussed in Section 6b.

Electrode-heating and surface-coating effects will also be considered. Work will be initiated on the surface/sheath behavior with particular attention to anode current saturation levels and toward developing techniques to characterize the electrode surfaces in the generator.

3. Breakdown Experiments and New Insulator Designs

Experimental and analytical work relating to axial breakdown has established that breakdown can be initiated either in the plasma or in the insulator. For many electrode wall designs, the threshold voltage for insulator-initiated breakdown is considerably lower than that for formation of an axial arc in the plasma, and the development of electrode wall designs with higher threshold levels is clearly desirable. The main purpose of the initial breakdown experiment and related analytical work is to produce an electrode/insulator design with a high threshold voltage, but with a relatively high overall surface temperature at operating conditions. The difficulty in obtaining such a design, indicated schematically in Figure

22, results from the high local Joule heating of the electrode edge at which the current concentrates. Narrow insulators, well cooled by the adjacent electrode elements, represent one possible solution; however, at high Hall parameters, narrow gaps may result in significant leakage in the absence of breakdown and may still result in insulator overheating due to the highly skewed current distribution.

As one possibility of improved insulator design, a "laminated" insulator is being considered. As shown in Figure 23, interelectrode insulation is provided by alternating slabs of metal and insulator, with the metal slabs providing more effective cooling of the crucial insulator regions. Preliminary heat-transfer calculations have been performed for the gas-side behavior and will be linked to a heat-conduction model in the next reporting period. The initial experiment will evaluate such a possibility. This experiment should be completed in the next reporting period.

4. Recession Rates in Inter-Electrode Insulators

Inter-electrode arcs have the potential to cause considerable damage to the generator wall. In some experiments such arcs were observed to be driven into the anode wall (presumably under the influence of the $\vec{J} \times \vec{B}$ force) and extinguished only upon reaching a well-cooled channel containment shell. In other experiments, inter-electrode arcing occurs without widespread damage even after relatively long term operation. Early considerations at Avco established some rules of thumb about the arc power level required to produce damage to their type of electrode wall. The purpose of the experiments and analysis in this subtask is to study the extent of damage to the walls from inter-electrode arcs and to develop designs and techniques to minimize the effects of such arcing.

For this purpose a small facility, shown schematically in Figure 24, is being assembled. Since the arc behavior after some penetration to the walls is to be studied, the plasma flow will first be simulated by an unseeded combustion products flow from a gaseous fuel torch as shown in the figure. A stagnation-flow configuration is used to obtain high heat transfer rates and bring the electrode module surface temperatures to the proper range. An arc can be initiated either by applying a voltage in excess of the threshold voltage for insulator-initiated breakdown or using "exploding wire" techniques to initiate a gaseous arc. A conventional iron-core magnet can provide a magnetic field strength as high as 2 Tesla allowing the influence of the $\vec{J} \times \vec{B}$ force to be investigated.

In the first reporting period all major components, except the high-voltage power supply, have been procured and a major portion of the installation completed. Results will be presented in the next progress report.

5a. Breakdown and Arc Onset on Slagged, Polarized Electrodes

The object of this subtask is to develop a small separate rig where slag-covered electrodes can be subjected to current passage under controlled conditions prior to their insertion in the MHD simulation facility in order to study the effects of polarization on the nature of the electrical plasma-slag discharge. A second objective is the characterization of the slag itself as a conductor as a function of temperature, history, polarity, and nature of the solid electrode.

Preliminary Considerations. The partially ionic nature of current transport in slag and in most ceramics is known to produce both an electrical polarization of these conductors under DC current and an enhanced rate of corrosion and erosion of the underlying electrodes or the current leadouts (15)(16)(17). Briefly, the general observations of several researchers are:

(1) Oxygen and/or sulfur negative ions are discharged at the interface between a metallic anode and its slag layer. This leads to severe oxidation of steel or copper anodes (without appreciable electrical changes), and to pitting and scoring of noble metal anodes, possibly by cavitation (with large accompanying polarization effects).

(2) These anode polarization effects consist of a gradual increase of the slag layer resistance over a period of minutes (faster at higher temperatures), leading ultimately to arcing. The interface between slag and metal is found to show a layer depleted in iron and enriched in Si and Al.

(3) Iron globules are found in the slag covering a cathode, mostly at the liquid-solid interface and near the metal itself. Little polarization is observed on cathodes, if anything showing a decreasing voltage drop.

(4) A characteristic shorting phenomenon occurring in the cathode wall of slagging MHD generators appears to be directly related to this cathodic slag reduction.

(5) Anode and cathode arcing on slag layers appears to be qualitatively different; in an anode, Koester et al⁽¹⁸⁾⁽¹⁵⁾ found a correlation between arcing threshold and (polarized) slag layer resistance, indicating that the bottom of the layer is controlling. On a cathode, observations at AVCO⁽¹⁹⁾ indicate that the arc is superficial, and does not usually penetrate the layer (the current may actually enter the electrode at a location quite removed from the arc attachment point).

A complete theoretical understanding of these and related phenomena is lacking, due to the very complex physico-chemical situation involved. This lack of understanding hampers the efforts to develop methods and strategies to cope with their deleterious effects; until quite recently, for instance, conductivity data for slags were quoted without much attention to whether DC or AC tests were involved, and electrode design criteria were based on no-current corrosion limits. This situation has been reversed now, but basic theoretical frameworks and data are still to be obtained.

Ref. (16) presents a simple model for conduction in slags and ceramics. A basic conclusion is that ion-blocking electrodes are to be avoided, as they lead to extremely oxidizing or reducing local conditions that tend to deteriorate materials. While this is substantiated by the above discussed experimental observations, the detailed mechanism involved may differ from that postulated. In particular, Ref. (16) determines an equilibrium oxygen potential distribution under current from the condition of zero ionic current; this condition must indeed be met at the blocking electrode, but not necessarily everywhere else in the conductor, since ions can be created or

discharged very near the blocking section. In fact, the size of this ionizing layer must be such that ion back-diffusion may balance the field-driven ion flux, and this implies a voltage across it of no more than a few kT (a fraction of a volt). In moderately resistive slags, this is small compared to the total layer voltage.

An alternative mechanism is postulated in Ref.(20). At a blocking anodic interface, metallic cations are created, which migrate down the potential gradient, leaving behind a depleted, oxidized material, with a lower electronic conductivity. This new barrier layer continues to grow as long as current is passing, and arcing or other instabilities do not occur. For a fixed current density, an arcing voltage is eventually reached; for a fixed applied voltage, the current decreases exponentially; for a linearly increasing voltage, the current reaches a steady level. This mechanism conforms well to the observations of Refs.(15) and (17), which noticed Fe and K depletion near the anode walls. Notice that, contrary to Ref.(16), no steady or equilibrium state is reached here for constant current.

At the interface to an oxidizable material, such as mild steel⁽¹⁵⁾, the cations originate from this material itself, and the oxide layer penetrates the electrode without metal depletion and hence without much polarization. Oxygen anions may also contribute to the anodic current, again forming oxides with the electrode metal as they are discharged. At the plasma-slag interface at the anode, oxygen is dissolved from the plasma as required, while arriving iron or potassium cations neutralize and may then vaporize to maintain equilibrium with the gas.

Similar, but reversed mechanisms seem to act in the cathode slag. Iron ions form at the plasma interface and discharge either on the metal

surface or wherever ionic conductivity is replaced by electron or hole conduction. This seems to occur, at least partially, at the transition from liquid to solid slag (about 1300-1500°K)⁽¹⁷⁾. Oxygen ions may also migrate from these regions to the plasma interface, leading to surface bubbling or simply to evaporation. Since metal accumulation tends to enhance the conductivity, no increase in voltage drop is found in cathode slag layers.

The anodic polarization is controlled, according to the above picture, by the dependence of conductivity on oxygen potential in the depleted barrier layer. It is instructive to examine this dependence using a simple equilibrium, isothermal model, with the following assumptions:

(1) Charge carriers are electrons, holes and divalent positive (F_e^{++}) and negative ($O^{=}$) ions. Local neutrality demands

$$n_e + 2n_- = n_h + 2n_+ \quad (52)$$

(2) At the given temperature, electron-hole equilibrium gives

$$n_e n_h = G(T) \quad (53)$$

(3) Similarly, equilibrium of the ion-formation reaction gives

$$n_+ n_- = K_i(T) \quad (54)$$

(4) Also, equilibrium of the (potential) molecular oxygen with its ions gives

$$\frac{n_-}{n_e^2 n_{O_2}^{1/2}} = K_o \quad (55)$$

(5) In terms of the mobilities B_e, B_h, B_+, B_- , the overall conductivity and the electron (p, n) conductivity are

$$\sigma_{el} = e(n_e B_e + n_h B_h) \quad (56)$$

$$\sigma = 2e(n_- B_- + n_+ B_+) + \sigma_{el} \quad (57)$$

Combining Eqs.(52)-(55), the electron density and the oxygen potential are related by the equation

$$n_e + 2K_o n_{o_2}^{1/2} n_e^2 = \frac{G}{n_e} + \frac{2K_i}{K_o n_{o_2}^{1/2} n_e^2} \quad (58)$$

Thus, given n_{o_2} , n_e is determined, and then all other densities, and the conductivities, follow easily. There are four terms in Eq.(58), and this determines several regimes, depending on the oxygen potential and the other parameters. A schematic plot of Eq.(58) is shown in Fig. 25, where these regimes are clearly exhibited.

In Fig.25 the important dividing points A, B, C are:

$$(A): n_e = n_- ; n_{o_2} = \frac{1}{16 K_o^2 K_i^2}$$

$$(B): n_e = n_h ; n_- = n_+ ; n_{o_2} = \frac{K_i^2}{G^2 K_o^2}$$

$$(C): n_h = n_+ ; n_{o_2} = \frac{16 K_i^3}{G^4 K_o^2}$$

Since $n_h = G/n_e$, the electronic conductivity is seen to vary within each regime as $\sigma_{el} \approx \sigma_o P_{o_2}^{-1/m} + \sigma_h P_{o_2}^{1/n}$, a representation often used (Ref.16). The particular combination of exponents depends on the range of P_{o_2} and the relative mobilities B_e, B_h . In particular, the data for $x L_{a_{0.75}} S_{r_{0.25}} F_{e_{0.3}} O_3 - (1-x) S_{r_{0.25}} Z_{r_{0.3}} O_3$ given in Ref.(16) correspond to a case where $B_h > B_e$; there is a minimum of σ_{el} , and for lower P_{o_2} , where electrons predominate, $\sigma_{el} \sim P_{o_2}^{-1/6}$, while above the minimum, $\sigma_{el} \sim P_{o_2}^{1/4}$ due to positive and negative ions.

Data to calculate the constants of the model for slags are not available (except partially). However, the general trend is in any case towards a decrease of n_e as n_{O_2} increases. If we consider the slag near the metal interface in the anode to be initially to the left of point A in Fig. 25 the progressive iron depletion, with its implied increase of the oxygen potential, moves its composition to the right. Electron conductivity is thus decreased, and so is the positive ion contribution (like $n_{O_2}^{-1/2} n_e^{-2} \sim n_{O_2}^{-1/6}$); other contributions are negligible in this regime. This leads to the observed polarization of the layer.

If the initial point is between A and C (or, in any case, if this condition is reached from the left), polarization does not occur, since n_- and n_+ are nearly independent of n_{O_2} here. If the region to the right of C is involved, there is actual depolarization, as both n_h and n_- increase there as $n_{O_2}^{1/6}$. These considerations indicate that, at least at the higher (liquid) temperature, and for the usual MHD conditions, the oxygen potential in the slag must fall below the level for point A in Fig. 25.

Experimental Activity. The experimental work on this task has been so far only preliminary. A sample of slag collected from the MHD facility was plasma-sprayed on a stainless steel receptacle shaped as a channel 2.5 x 1 cm in area and ~4 mm deep. Current was supplied from a top steel electrode in contact with the slag; the whole assembly was heated with an oxy-acetylene torch to temperatures between 700 and 1200°K (determined from an embedded thermocouple). After an initial period, good electrical contact between steel and slag was established, and voltage-current data were taken at varying temperatures. Fig.26 shows these data for two runs, spaced nine minutes apart.

Even at the low temperatures of the test, there is a 25-30% increase of resistance in these nine minutes. This seems to indicate that it is possible to "run-in" the slagged electrodes at a moderate temperature for a longer period of time. Tests are planned at higher temperatures and with more controllable conditions to ascertain this point.

5b. Modeling of Slag Flow with Ripples

Introduction

In previous modeling of the fluid and electrical characteristics of the slag flow in MHD generators, simple smooth film models have been used. This approach consists of modeling the highly viscous layer flowing smoothly over the MHD channel walls under the influence of shear stress, pressure gradient, gravity force, etc. Beneath the flowing layer a frozen slag layer also exists. The high heat flux from the gas side causes a temperature gradient across the layer with the wall considerably colder than the surface. Due to the strong dependence of slag viscosity on temperature, the viscosity profile has a sharp gradient close to the surface of the layer. This gradient results in a higher surface velocity and smaller thickness for a given mass flow rate than if the viscosity were constant. The effects of Joule heating in the layer (over the electrodes) have also been included in such a model and the variations of thickness, surface velocity and surface temperature have been predicted. Experimental observations using cinephotography have shown, however, that the slag flow is not smooth but rather that ripples form transvers to the mass flow direction as shown in Figure 27. The crests of the ripples appear brighter than the surrounding slag presumably because they are at higher temperature. It appears that mass and current transport might be significantly affected by the ripple flow since both viscosity and electrical conductivity of slag are strong functions of temperature. In fact, visual observation of slag flow with current by the Soviets [21] confirms that current transport is in

fact affected at least for some conditions. Further, the ripples can affect the boundary layer profile and result in increased fluid shear and higher channel friction losses. These observations indicate the necessity of establishing precisely the role of the ripples in mass transport and current transport and of developing a new model for the slag flow.

The formation of ripples on thin viscous films has been the study of some investigations [22, 23] and even in the absence of surface shear and property variations (i.e. thin film flowing under the influence of gravity) represents a formidable problem. Ripple (or wave) generation under the application of shear [24, 25, 26] is even more difficult due to the complex flow in the vicinity of the ripples, as indicated schematically in Figure 27. Inclusion of the effects of variable viscosity, or Joule heating and of other phenomena which arise in the MHD situation can, in the initial stage, serve only to make unmanageable an already difficult problem. Further, the difficulty of performing diagnostic measurements (such as ripple peak and trough heights, surface temperature) under slagging wall MHD conditions suggests that experiments simulating the conditions would be desirable.

As part of Subtask 4 relating to slag electrical transport, a study of ripple formation and behavior has been initiated and the preliminary results are present in this section. The experimental approach followed is to simulate the slag flow by a suitably scaled flow of a highly viscous fluid under the influence of a flowing air stream. For this situation measurements of ripple size and shape, the various relevant velocities and mass flux are greatly simplified over the similar measurements in the combustion environment. Initial experiments have been carried out for constant viscosity and the results compared against the smooth film results.

Design of an improved experimental apparatus which will allow measurements at higher shear stress and with a temperature (viscosity) gradient transverse to the layer is discussed. Such experiments will be performed in the next reporting period.

Preliminary Experimental Results and Interpretation

The purpose of these experiments is to simulate the most important features of the slag flow in MHD channels using a room temperature air flow and a suitable viscous fluid. Initially, the results from such experiments can be used as empirical data from which the slag flow behavior can be determined and the effects of the ripples on the electrical and fluid behavior in the plasma can be evaluated. Of more importance, however, the experiments can be used to guide the development of models of the ripple formation and the slag flow with ripples. From these models the importance of ripples on the overall fluid and electrical performance of the channel can be determined. The apparatus utilized in the preliminary experiments is shown schematically in Figure 28. A blower is used to provide air flow through the test section after suitable flow straightening. The glycerine from an elevated reservoir is introduced through a slot in the bottom plate and flows downstream under the influence of the fluid shear. The mass flow of glycerine is adjusted by changing the height of the reservoir and the shear is changed by adjusting the motor speed. Visual observations of the viscous layer represent the main experimental data. Ripple height, breadth, separation and speed are measured as well as the trough height. In some experiments particles were suspended in the glycerine and used to monitor the flow within the fluid film.

Dimensional Analysis Considerations

The major reason for this subtask is to provide a good understanding of the fluid and heat transfer characteristics of the slag film without facing measurement complications in a "laboratory" MHD generator. For the present, only the fluid behavior of the viscous film is considered; that is, the film is at constant temperature and no electric or magnetic fields are present. The important parameters are outlined in Figure 29. The gas and film flow are assumed to be two-dimensional and the gas flow is assumed to be incompressible. A comparison of the parameters for MHD conditions and for the simulation experiments is shown in Table 5.

In the preliminary experiments (and in the proposed experiments) the boundary layer thickness is small enough that the flow can be considered two-dimensional. For the film side it is convenient to characterize the interaction with the gas side by an effective surface shear stress and to then carry out separate similarity considerations for the gas side and the fluid side. In this way, the principal expected dependence of the effective shear on the parameters of importance can be substituted and some correlation of the data achieved. From this point of view, the slag surface represents a rough wall to the gas flow and one expects

$$\frac{\tau_{\text{eff}}}{\rho U_{\infty}^2} = f(\text{Re}_{\infty}, E_1/\delta, E_2/\delta) \quad (52)$$

where δ is the boundary layer thickness and E_i are the dimensions characterizing the effective wall roughness. To the extent that the surface character remains constant, we might take

$$\frac{\tau_{\text{eff}}}{2} \propto \bar{\beta} / \text{Re}_{\infty}^{2n} \quad (53)$$

$$\rho U_{\infty}$$

where n is 0.25 for laminar flow and 0.1 for turbulent flow and β^- is hopefully only a weak function of the surface shape. Note that use of the intermediate effective shear is not necessary from the point of view of dimensional analysis, but is rather an attempt at characterizing the interaction of fluid and gas. The resulting dimensionless groups become

$$\begin{aligned}\pi_1 &= Re_{\infty} = \frac{\rho_{\infty} U_{\infty} L}{\mu_{\infty}} \\ \pi_2 &= \frac{\mu_s}{\mu_{\infty}} \\ \pi_3 &= \frac{\dot{m}}{\mu_s} \\ \pi_4 &= \frac{\rho_s^{1/3} g^{2/3} \dot{m}^{2/3}}{\rho_{\infty} U_{\infty}^2} Re_{\infty}^{2n} \\ \pi_5 &= \frac{\rho_s}{\rho_{\infty}} \\ \pi_6 &= \frac{\rho_s \sigma_s R_{e\infty}^n}{\rho_{\infty} \dot{m} U} \\ \pi_7 &= \frac{P_{\infty}}{\rho_{\infty} U_{\infty}^2}\end{aligned}$$

where $n = .25$ for laminar flow and 0.1 for turbulent flow. Measured film velocities and length scales are non-dimensionalized as

$$\begin{aligned}\pi_u &= \sqrt{\frac{\rho_s u}{\rho_{\infty} U}} Re_{\infty}^n \\ \pi_p &= \frac{\rho_s \rho_{\infty} U_{\infty}^2 p^2}{\dot{m}^2 Re_{\infty}^{2n}}\end{aligned}$$

Preliminary Results

For all conditions of the preliminary experiments, transverse ripples, similar to those present in slag experiments, were observed. The variation of the dimensionless ripple wavelength, height and speed and trough height with π_3 or π_4 are shown in Figures 30a - 30d. The film thickness was much smaller than what would be expected from the smooth film model in contrast to the ripple speeds which were very high compared to the surface velocities expected in the smooth film case. In addition, the importance of the ripples increased significantly within μ/μ_s (keeping all other parameters constant) to a level at which most of the liquid seemed to prefer the ripple structure: ripples gained higher speeds and became more closely spaced. Although these results are preliminary and do not span the range of the appropriate dimensionless numbers, they are encouraging in that the expected qualitative behavior was observed and that the measurements are consistent.

Values for the various dimensionless parameters are shown in Table 6, for MHD conditions and for the simulation rigs. The wide range for $\pi_3 = \dot{m}/\mu$ results because of uncertainties in the slag carryover and from the variation of slag viscosity with temperature and slag type. It may be necessary to use viscous fluids other than glycerine to cover the entire range expected for π_3 .

Advanced Experimental Rig and Planned Experiments

A second experimental rig was designed after experience was gained using the first rig. The second channel, shown schematically in Figure 31,

is designed to accomodate higher gas velocities and to allow cooling of the surface beneath the film to simulate the viscosity gradients present in the MHD configuration. The channel consists of a feeding section and removable channel sections, with a maximum length of ~6 feet. The top and sidewalls of the channel are plexiglas while the bottom wall is made of aluminum. Copper tubes will be soldered to the copper plate and the plate cooled to the desired temperature. Gravity feed of the viscous fluid will be used. Ports are provided along the test section length to allow measurement of gas velocity profiles and pressure measurements. Ripple size, shape and velocity will be measured visually. Fluid motion interior to the ripple will be recorded by seeding the fluid with particles and photographing the motion of the particles in the central region of the channel. Together with the new blower (allowing air speeds greater than 50 m/sec) the new experimental apparatus should allow the studies to be extended to conditions typical of MHD channel operation.

In the present reporting period, the new blower has been procured and installed and the new channel designed, constructed and partially assembled. Experimental results will be obtained in the next reporting period. Analytical efforts in the next reporting period will be extended from the simple dimensional analysis considerations. It is expected that an integral approach to the slag flow will be pursued, with experimental results or simple analytical techniques used to determine the effective shear exerted on the film layer from the measured surface shape.

6. Support Activities

6a. Facility Upgrading and Operation

In addition to making the MHD Simulation facility available for the main experiment, many modifications and improvements have been effected during the reporting period. These are discussed in Task 3, Part B of the report.

6b. Plasma Relative Temperature Measurement Array

In support of the arc study effort, a simple prototype of a system to measure the time-resolved spatial distribution of plasma temperature in the boundary layer in a MHD channel has been designed and fabricated. The basic measurement is of the emission of a potassium line (404 nm), integrated along a very narrow line of sight parallel to the electrode wall.

The prototype sensor head consists of a 2 x 4 array of discrete 200 μm diameter quartz optical fibers on 1 mm centers with provision for spatial filtering so as to get substantially pure line-of-sight response. The fibers are 3 m long and are enclosed in a rugged protective sheath leading to the detector subsystem (see Figure 32). At the detector end, the eight fibers are arranged in a 1 x 8 linear array and imaged through a 10:1 reduction onto eight alternate elements of a 128-element self-scanning Reticon photodiode array. This array may be read out at MH_z rates, given a sufficient light level, and will be coupled to an existing 4 MH_z digital transient recording system, which is in turn coupled to the existing computer-based data acquisition and analysis system.

It is expected that this system will allow on the order of 10 "snapshots" of the plasma relative temperature distribution to be obtained during the millisecond time interval characteristic of arc formation.

6c. Modeling and Analysis

The major modeling effort in this report has been in support of subtask 1 and has already been discussed in that section. In preparation for the inclusion of electron thermal non-equilibrium effects in the boundary layer code the existing equilibrium composition and transport properties program has been modified to compute electrical conductivity and electron number density for $T_e = T_g$. In addition, the routine has been made such that the gas constituents can be more easily changed.

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Figure 1. Layout of M.H.D. Simulation Facility

Figure 2b. Channel Assembly. Top View.

Figure 3. Cross-sectional View of Channel showing double layer of MgO brick liner.

Figure 4. Electrode Module Assembled and in Place:
View through side cover.

View Through Electrode Wall

View Through Side Cover
"B" Side Only

Figure 5a. Schematic arrangement and labeling of voltage probes, electrodes and currents in channel.

Figure 5b. Cross-sectional View of Channel showing placement of Voltage Probes.

(a)

(b)

Figure 6a. Measurement of Probe Voltages.

Figure 6b. Measurement of Electrode Currents by use of a Series Resistor.

RUN # = 108

BURNER PARAMETERS

BURNER PRESSURE= 1.08 PSIG JP4 FLOW RATE=0.0575 LBS/SEC
 OXY FLOW RATE=0.1438 LBS/SEC AIR FLOW RATE=0.2643 LBS/SEC N2/O2= 0.960
 KOH + H2O FLOW RATE= 0.016 LBS/SEC (45% KOH BY WEIGHT) SEED FREACTION=0.0105

CHANNEL PARAMETERS

CHANNEL PRESSURE= 0.24 PSIG
 NG VOLTAGE= 59.25 VOLTS
 TOTAL IA= 13.22 AMPS TOTAL IC= 13.17 AMPS
 IAA= 4.76 ICA= 3.43 VA= 73.47 TOTAL V= 81.66
 IAB= 4.01 ICB= 5.62 VB= 76.97 TOTAL V= 86.60
 IAC= 4.45 ICC= 4.12 VC= 73.09 TOTAL V= 81.66

VOLTAGE PROFILE (VOLTS)

CATHODE										ANODE
B1-B2	B2-B3	B3-B4	B4-B5	B5-B6	B6-B7	B7-B8	B8-B9	B9-B10	B10-B11	
22.87	1.32	3.71	4.24	3.87	4.46	5.19	8.15	9.83	7.99	
B1-BB2	BB2-BB3	BB3-BB4	BB4-BB5	BB5-BB6	BB6-BB7	BB7-BB8	BB8-BB9	BB9-BB10	BB10-BB11	
20.20	1.35	6.21	5.18	4.56	4.55	5.39	6.54	5.39	14.08	

CONSISTENCY CHECKS (VOLTS)

SUM B= 71.64	SUM BB= 73.45
SUM B= 71.64	B1-B11= 71.26
SUM B6-B11= 35.62	B6-B11= 35.08
SUM B3-B9 = 29.63	B3-B9 = 30.20
SUM B1-BB5= 32.94	B1-BB5= 30.76
SUM B9-B11= 17.62	B9-B11= 17.49
SUM BB9-B11= 19.47	BB9-B11= 19.78
SUM B5-B7= 8.33	B5-B7= 8.38
SUM BB4-BB8= 19.67	BB4-BB8= 18.80

Figure 7a. Summary Listing of Data Taken for Run 108.

RUN # = 118

BURNER PARAMETERS

 BURNER PRESSURE= 1.12 PSIG JP4 FLOW RATE=0.0571 LBS/SEC
 OXY FLOW RATE=0.1433 LBS/SEC AIR FLOW RATE=0.2637 LBS/SEC N2/O2= 0.958
 KOH + H2O FLOW RATE= 0.016 LBS/SEC (45% KOH BY WEIGHT) SEED FREACTION=0.0105

CHANNEL PARAMETERS

 CHANNEL PRESSURE= 0.25 PSIG
 MG VOLTAGE=182.60 VOLTS
 TOTAL IA= 53.90 AMPS TOTAL IC= 53.77 AMPS
 IAA= 19.17 ICA= 14.61 VA=219.62 TOTAL V=252.41
 IAB= 15.59 ICB= 21.05 VB=233.96 TOTAL V=270.60
 IAC= 19.14 ICC= 19.11 VC=215.43 TOTAL V=252.68

VOLTAGE PROFILE (VOLTS)

CATHODE									ANODE
B1-B2	B2-B3	B3-B4	B4-B5	B5-B6	B6-B7	B7-B8	B8-B9	B9-B10	B10-B11
39.53	7.74	17.05	18.32	16.37	18.31	20.43	28.96	27.91	21.80
B1-BB2	BB2-BB3	BB3-BB4	BB4-BB5	BB5-BB6	BB6-BB7	BB7-BB8	BB8-BB9	BB9-BB10	BB10-BB11
33.66	10.19	15.25	21.85	18.27	18.70	21.90	26.67	18.00	30.59

CONSISTENCY CHECKS (VOLTS)

SUM B=216.31	SUM BB=215.09
SUM B=216.31	B1-B11=217.01
SUM B6-B11=117.31	B6-B11=117.56
SUM B3-B9 =119.43	B3-B9 =122.46
SUM B1-BB5= 80.96	B1-BB5= 79.31
SUM B9-B11= 49.61	B9-B11= 49.03
SUM BB9-B11= 48.59	BB9-B11= 49.87
SUM B5-B7= 34.68	B5-B7= 34.95
SUM BB4-BB8= 80.72	BB4-BB8= 79.41

Figure 7b. Summary Listing for Data Taking for Run 118.

Figure 8a. Voltage Profiles from Data of Run 108.

Figure 8b. Voltage Profiles from Data of Run 118.

Fig. 9a. Anode Load Line for Conditions Numbers 100

Fig. 9b. Cathode Load Line for Condition Numbers 200

Fig. 10a. Anode Load Line for Condition Nubers 200

Fig. 10b. Cathode Line for Condition Numbers 100

Figure 11a. Geometry for Analysis of Electrical Measurements.

Figure 11b; Non-uniformities in Current Transfer

Figure 12. One dimensional discharge configuration for boundary layer analysis.

Axial Momentum:

$$0 = -u \frac{\partial u}{\partial x} - v \frac{\partial u}{\partial y} - \frac{1}{\rho} \frac{dp}{dx} + \frac{1}{\rho} \frac{\partial}{\partial y} (\mu + \rho \epsilon_m) \frac{\partial u}{\partial y}$$
$$\left. \begin{aligned} u(x,0) &= 0 \\ u(x,H) &= u_\infty \\ p(x,y) &= p(x) \end{aligned} \right\} - \frac{dp}{dx} = \frac{\rho}{2} \frac{du_\infty^2}{dx} ; \quad p(x) \text{ specified}$$

Energy:

$$0 = \frac{J^2}{\rho \sigma} - u \frac{\partial h}{\partial x} - v \frac{\partial h}{\partial y} + \frac{1}{\rho} \frac{\partial}{\partial y} \left(\frac{k}{c_p} + \rho \epsilon_H \right) \frac{\partial h}{\partial y} + \frac{u}{\rho} \frac{dp}{dx} + \left(\frac{\mu + \rho \epsilon_H}{\rho} \right) \left(\frac{\partial u}{\partial y} \right)^2 + u (\epsilon_H + \epsilon_m) \frac{\partial^2 u}{\partial y^2} + \frac{u}{\rho} \frac{\partial u}{\partial y} \frac{\partial}{\partial y} (\rho \epsilon_H - \rho \epsilon_m)$$
$$h(x,0) = h_{\text{wall}}(x); \text{ specified}$$
$$h(x,H) = h_\infty ; \text{ specified}$$
$$J(x,y) = U(x); \text{ specified}$$

Continuity:

$$v(x,y) = - \frac{1}{\rho} \int_0^y \frac{\partial}{\partial x} [\rho(x,\xi) u(x,\xi)] d\xi$$

Electron-Ion Continuity:

$$0 = -u \frac{\partial c_e}{\partial x} - v \frac{\partial c_e}{\partial y} + \frac{1}{\rho} \frac{\partial}{\partial y} (\rho D_a + \rho \epsilon_E) \frac{\partial c_e}{\partial y} + \frac{m_e}{\rho} \dot{n}_e$$
$$c_e(x,0) = c_e^*(x) \approx 0$$
$$c_e \equiv \frac{n_e m_e}{\rho} \quad (\text{mass fraction})$$
$$c_e(x,H) = c_{e\infty}^* ; \text{ specified}$$
$$\dot{n}_e = 2.52 \times 10^{-33} n_e \exp(-2.98 \times 10^{-3} T) (n_e^{*2} - n_e^2)$$

where the * denotes equilibrium.

Electron Heating:

$$\frac{3}{2} k (T_e - T_h) 2 \delta^* \frac{m_e}{m_h} \bar{v}_{eh} = \frac{1}{n_e} \frac{J^2}{\sigma_e}$$

Figure 13a. Summary of equations, boundary and initial conditions used in modeling.

Transverse Momentum:

$$\frac{1}{\rho} \frac{\partial p}{\partial y} \approx -u \frac{\partial v}{\partial x} - v \frac{\partial v}{\partial y} + \frac{1}{\rho} \frac{\partial}{\partial y} \mu \frac{\partial v}{\partial y}$$

$v(x,y)$, $u(x,y)$ from solution of above equations

Turbulence Modeling:

Momentum Transport

$$\epsilon_m = \ell_m^2 D^2 \left| \frac{\partial u}{\partial y} \right|$$

$$\ell_m = \begin{cases} Ky & \text{for } Ky \leq \lambda_m \delta \\ (K\lambda_m \delta) & \text{for } Ky > \lambda_m \delta \end{cases}$$

$$D = 1 - \exp(-y^+/A_o^+)$$

where

$$y^+ = y \sqrt{\rho \tau_w / \mu}$$

$$\tau_w = \mu \left. \frac{\partial u}{\partial y} \right|_{\text{wall}}$$

and

$$K = .41, A_o^+ = 24, \lambda_m = .085 \text{ or } = 1.0 [9]$$

Energy and Species Transport

$$\epsilon_e = \epsilon_H = \ell_H^2 D^2 \left| \frac{\partial u}{\partial y} \right|$$

$$\ell_H = \begin{cases} Ky & \text{for } Ky \leq \lambda_H \delta \\ (K\lambda_H \delta) & \text{for } Ky > \lambda_H \delta \end{cases}$$

D given above and

$$K = .41, \lambda_H = .085$$

Fluid properties stored on disk on PDP 11/60

Figure 13b. Summary of Equations, boundary and initial conditions used in modeling.

Figure 14. Comparison of calculated electron number density computed by boundary layer code to simple model profiles.

Figure 15. Comparison of calculated static enthalpy profile computed by boundary layer code to simple model profiles.

Figure 16. Comparison of frozen electron density profile to the calculated kinetic profile.

Figure 17. Departure of electron density from equilibrium in boundary layer.

Figure 18. Comparison of model frozen profiles to calculated kinetic profile.

Fig. 19a. Comparison of calculated boundary layer load line to experimental results. There is electron temperature equilibrium and the recombination mechanism is electron-third-body.

Fig. 19b. Comparison of calculated boundary layer load line to experimental results. There is electron temperature equilibrium and the recombination mechanism is electron-third-body.

Fig. 20a. Comparison of calculated boundary layer load line to experimental results. There is electron temperature equilibrium and both electron and heavy particle third-body recombination are considered.

Fig. 20b. Comparison of calculated boundary layer load line to experimental results. There is electron temperature equilibrium and both electron and heavy particle third-body recombination are considered.

Fig. 21. Plot of Heavy Gas and Electron Temperatures for Typical Condition of Experiment and with $A_{mm} = 1.0$ Boundary Layer Thickness of Approximately 5 mm.

Fig. 22a Schematic of Axial Configuration used in Breakdown Experiments Showing the Current Pattern and the Regions of High Heat Flux

Fig. 22b Schematic of Induced Field Configuration Showing the Region of High Heat Flux

Fig. 23. Schematic of Proposed Inter-electrode Insulator Gap Configuration to Alleviate the Heating at the Regions of High Heat Flux.

Fig. 24. Schematic of Experimental Rig to Simulate Damage Caused by Inter-electrode Arcs (Torch is used to simulate temperature variation in wall components.)

Fig. 25. Dependence of Electron Density on Potential Oxygen Density

Figure 26. Slag Resistance Vs. Temperature,
Showing Polarization Effect.

Fig. 27. Schematic Representation of a Slag Layer over MHD Channel Wall
(Profiles not to Scale)

Fig. 28. Schematic Representation of the Preliminary Channel

Gas Side Parameters

U_∞ - Freestream Velocity
 ρ_∞ - Freestream Density
 μ_∞ - Freestream Viscosity

Slag Parameters

ρ_s - Slag Density
 μ_s - Slag Viscosity
 m - Slag Main Flux
 σ_s - Surface Tension

L - Characteristic length for measurements

g - Gravity force/unit volume

Fig. 29. Dimensional Similarity for the Gas Side and Film Side

Fig. 30a. Plot of Non-dimensional Ripple Speed vs Non-dimensional Mass Flow Rate Parameter (Sketch Identifies Geometry and

Fig. 30b. Plot of Non-dimensional Ripple Wavelength as a Function of Non-dimensional Mass Flow Rate Parameter

Fig. 30c. Plot of Non-dimensional Ripple Height vs Non-dimensional Gravity Parameter, π_4

Fig. 30d. Plot of Non-dimensional Trough Height as a Function of Non-dimensional Gravity Parameter, π_4

Fig. 31. Schematic of Second Experimental Rig for Simulation of Slag Flow in MHD Generators

Figure 32. Fiber-optics assembly for relative plasma temperature measurement (full scale).

Condition #	Nominal Flow Conditions		Purpose
	<u>N₂/O₂</u>	<u>Seed</u>	
10	1.5	NO	Thermal
20	1.0	NO	Thermal
30	1.0	YES	Thermal
100's	1.0	YES	Electrical (anode visible)
200's	1.0	YES	Electrical (cathode visible)
300	0.8	NO	Thermal
310	0.5	NO	Thermal
400's	0.5	YES	Electrical (anode visible)
500's	0.5	YES	Electrical (cathode visible)
600	-	NO	Shutdown

Table 1.

Nominal Combustor and Electrical Conditions for Current
Transfer Experiment.

Component Temperatures

Condition #	T _{electrode surface}	T _{brick surface}
10	1020 K	1660 K
20	1230 K	1940 K
100's	1150 K	2200 K
200's	1150 K	2200 K

Combustor Heat Loss

Condition #	Heat Loss
100's	8.13×10^5 J/kg of total mass flow
200's	8.13×10^5 J/kg of total mass flow

Table 2

Thermal Data Obtained from Current Transfer Experiment

Condition #'s 100

$T_{\text{electrode wall}}$	=	1150°K
$T_{\text{brick wall}}$	=	2200°K
T_{core}	=	2572°K
P	=	1.03 atm
σ_c	=	6.81 mhos/m
$\langle \sigma_z \rangle$	=	6.55 mhos/m

Condition #'s 200

$T_{\text{electrode wall}}$	=	1150°K
$T_{\text{brick wall}}$	=	2200°K
T_{core}	=	2584°K
P	=	1.03 atm
σ_c	=	7.26 mhos/m
$\langle \sigma_z \rangle$	=	7.00 mhos/m

Table 3. Conditions for the numbers 100 and numbers 200 Series of runs.

□ Results of boundary layer code

▨ "BB" side voltage probes

▨ "B" side voltage probes

Current carrying surface area of electrodes
is 7.2 cm^2

Table 4. Voltage probe configuration for load line graphs.

Table 5

Comparison of Fluid Related Parameters for Slagging MHD and Flow Simulation

Rig

	MHD Channel		Slag Flow Simulation Rig (Using glycerine)	
gas side	Central Station	"Laboratory"	Preliminary	Rig 2
U_{∞} m/sec	1000	1000	≤ 20	≤ 50
ρ_{∞} Rg/m ³	0.1 - 0.5	0.1 - 0.2	1.27	1.27 kg
μ_{∞} Rg/m-s	8×10^{-5}	8×10^{-5}	1.8×10^{-5}	1.8×10^{-5}
L m	5	1	0.1	1.0
Re_x @ .1 m	1.3×10^6	$.5 \times 10^6$	$\leq .13 \times 10^6$	$\leq .34 \times 10^6$
Re_L @ L	6.3×10^6	1.3×10^6	$\leq .13 \times 10^6$	$\leq 3.4 \times 10^6$
film side				
ρ_s kg/m ³	2500	2500	1260	1260
μ_s kg/m-s	10 - 300	10 - 300	1.5	1.5
$\dot{m}'$ Rg/m-s	$10^{-3} - 10^{-1}$	$\sim 10^{-4}$	$10^{-3} - 10^{-1}$	$10^{-4} - 10^{-1}$

Table 6

Ranges of the Independent Dimensionless Groups

	Central Station MHD	"Laboratory" MHD	Slag Flow Simulator	
			Preliminary	"Rig 2"
$\pi_1 = \frac{\rho_\infty U_\infty L}{\mu_\infty}$	6.3×10^6	1.3×10^6	$\leq .13 \times 10^6$	$\leq 3.4 \times 10^6$
$\pi_2 = \mu_s / \mu_\infty$	1.25×10^5 3.8×10^6	1.25×10^5 3.8×10^6	$.83 \times 10^5$	$.83 \times 10^5$
$\pi_3 = \dot{m}^1 / \mu_3$	$10^{-6} - 10^{-2}$	$10^{-7} - 10^{-5}$	$10^{-3} - 10^{-1}$	$10^{-4} - 10^{-1}$
$\pi_4 = \frac{\rho_s^{1/3} g^{2/3} \dot{m}^{2/3} Re^{2n}}{\rho_\infty U_\infty^2}$	3×10^{-3} -.35	1.7×10^{-3} 3.5×10^{-3}	.35 - 7.7	.06 - 6.3
$\pi_5 = \rho_s / \rho_\infty$	$2.5 \times 10^4 -$ 1.25×10^5	$2.5 \times 10^4 -$ 5.0×10^4	10^3	10^3

TASK 2. OPEN CYCLE DISK GENERATOR PROGRAM

INTRODUCTION

The potential of the disk generator with swirl as an alternative to a linear channel for high performance MHD power generation is manifested by both (a) the inherent advantages as a result of the circular symmetry^[1] and (b) encouraging laboratory results from shock tunnel experiments^[2-8]. The disk configuration is attractive inasmuch as only a single electrode pair is used to extract the output current. Thus, duct life and performance are improved; many serious plasma and material limitations associated with multiple-electrode linear devices are eliminated, and higher electric fields and power densities can be obtained. Magnet and channel design are simplified, with potential cost and availability benefits. With inlet swirl, the disk is converted from a pure Hall-effect device to the circular equivalent of a diagonally connected linear channel; the result is a substantial increase in electrical efficiency for moderate values of Hall coefficients^[1].

In 1973, the design and construction of a small-scale shock tube-driven generator facility was initiated at MIT. The facility's major asset is versatility, both with respect to changing channel configuration and implementation of plasma diagnostic techniques. This is essential for (a) experimental definition and verification of generator modeling, leading to optimum performance channel design and (b) analyses and understanding of the interaction physics and effective plasma properties which primarily govern the generator performance.

Under ERDA/DOE sponsorship, from 1975 - 1978 (contract No. EF-76-C-01-2215), experiments have been conducted in this facility to

assess the performance, effective plasma properties, MHD interaction phenomenon and loss mechanisms in small (volume = 2 liters) disk generators. This work is described fully in Reference [8]; the scalability and applicability of the results to nonideal MHD environments is discussed. With 45° inlet swirl, encouraging performance results were obtained ($B = 3$ Tesla): in Argon, power densities from about 100 MW/m^3 at 2100°K to nearly 500 MW/m^3 at $T_o = 3400^\circ\text{K}$ were obtained with an enthalpy extraction of 5 percent and 17 percent, respectively^[2,3]. A maximum isentropic efficiency of 51% and a maximum electrical efficiency of 62% were also achieved^[3,4]. These are the highest power densities and efficiencies ever measured in an inert MHD generator operating in this range of temperatures. Electrical efficiencies at high interaction in pure N_2 exceeded 70 percent, thus verifying theoretical predictions^[5]. With molecular gas mixtures encountered in open-cycle systems ($\text{N}_2/\text{CO}_2/\text{H}_2$), over 30 MW/m^3 were obtained (in spite of chemical nonequilibrium limitations--see below), and E-fields as high as 38 kV/m (nearly a factor of 8 greater than the linear counterpart) and effective Hall coefficients up to 5.8 were measured^[8]. Experiments with large radial disks at AVCO have shown that over 15% of the gas enthalpy can be extracted with these gases^[7].

Critical Performance Factors/Issues

Figure 1 is a flow chart which illustrates a typical program leading to a detailed design of a combustion (steady-state) disk generator. We are concerned here (in Task 2) primarily with the performance characteristics of the loft. The critical factors which govern the performance are the effective plasma properties (used in Ohm's Law), the degree of both swirl and MHD interaction (including boundary layers and slag), electric stress limitations, and electrode design.

The two major causes for degradation of channel properties (whether linear or disk) are (1) plasma nonuniformities which reduce the effective conductivity and Hall parameter, and (2) chemical nonequilibrium in small-scale supersonic generators. The two types of nonuniformities encountered with combustion gases are small-scale turbulence generally emanating from the combustor and large-scale (anisotropic) nonuniformities. In the disk, the latter are produced, for example, by wakes associated with inlet swirl guide vanes. For effective Hall coefficients $\beta_{\text{eff}} \leq 2.0$, the good agreement between theory and experiment for the electrical efficiency suggests that the wakes have a relatively small effect on generator performance^[3-5]. At high Hall coefficients, however, the effects may be more significant^[9] (see Figure 2). Therefore, new methods for introducing swirl should be explored (e.g., high swirl combustor).

Chemical nonequilibrium will generally yield a low interaction and conductivity level in the channel through failure of the plasma to recover the original dissociation energy. This effect is clearly important in small-scale supersonic generators (including CDIF size disks) and has been observed in previous shock-tunnel tests with clean-fuel ($\text{N}_2/\text{CO}_2/\text{H}_2$) mixtures^[4,6]. Kinetics effects in commercial size disks are calculated to be minimal although not necessarily negligible^[8]. The catalytic effects of impurities/particulates on the recombination of CO-O in real combustion gas environments, however, must be determined.

In addition to the effects of nonuniformities and chemical nonequilibrium, other factors which influence generator performance are (a) Hall-field limitations (b) boundary layer and slag effects (c) inviscid flow distortion arising from magnetic field gradients and nonuniform inlet flow, and (d) unsteady effects such as magneto-fluid mechanic (MFM) instabilities

(up to now unobserved in small channel experiments) which may be important in large-scale channels^[10]. Because of present facility limitations, the effects of slag and MFM instabilities can only be studied theoretically.

Program Objectives

The main program goal is to investigate the critical performance issues and develop a detailed theoretical and experimental data base for disk generators to be used later to predict, with confidence, the performance and design of CDIF and commercial size disk channels. Specific objectives are:

- Investigate flow in a pure radial flow disk in order to (a) evaluate and provide generic information on the quantitative effects of well-defined non-uniformities on disk MHD generator performance, and (b) establish a baseline performance datum for use in the evaluation of inlet swirl-flow disk experiments.
- Evaluate the performance of a disk generator with inlet swirl; assess the effects of swirl-plus-wakes; and determine the extent that chemical kinetics influences the design and performance of small-scale supersonic generators.
- Provide mathematical modeling (and diagnostics) support to the experimental program; correlate models with experimental data; and improve existing models (as required) to ensure that a best-effort capability will be available for use in scale-up of disk generators.

WORK ACCOMPLISHED

During the last six months our major efforts have been directed primarily in three areas: (a) nonuniformities studies, (b) instrumentation and diagnostics development, and (c) two-dimensional modeling of disk channels.

(a) Nonuniformities Studies

The extent of degradation of channel performance as a result of plasma inhomogeneities (e.g., in some cases up to 60% in power density) depends strongly upon the type of nonuniformity, its geometry and orientation relative to the magnetic field.^[11] Moreover, the performance with a particular nonuniformity depends largely upon whether the plasma is considered to be of infinite extent, or bounded by a given electrode and channel geometry.^[12, 13] Since the effects of nonuniformities are more severe at high Hall coefficients, it is of particular importance to quantify these effects in the disk (which is primarily a high Hall parameter device).

Initial work has begun both with respect to modeling 2-D nonuniformity effects in the disk (see Section (c)) and designing a controlled experiment in which well-defined wake nonuniformities are studied. Wakes are created in the disk, for example, when guide vanes are used to produce inlet swirl. Numerical results obtained previously with a 2-D wake model in which the electrodynamic equations were solved for specified values of gas dynamic variables (typical of laboratory test conditions for a weak interaction) illustrate the effects of this type of nonuniformity on generator performance. These results are reported in detail in Reference 12 and are summarized as follows:

(1) The main effects of the wakes are to reduce the open-circuit voltage and maximum electrical efficiency (see Figure 2). These effects are stronger as the Hall coefficient, β , increases. The short-circuit current is reduced at most by 5%. Swirl offsets the degradation somewhat; however, at high β , a tradeoff exists between the extent of the wake effects and the increase in efficiency (see Figure 2b).

(2) The idealized parallel-layer model of Rosa^[14] overpredicts the degrading effects on performance. The constant potential electrodes (finite plasma boundary) have a uniformizing effect which, in the limit of 360 wakes, agrees with Rosa's model. This is illustrated in Table I where, for completeness, the results of an analytical "piecewise-constant" conductivity model are also shown.^[15]

The above numerical model has been extended to include the coupling of the electrodynamic and gasdynamic equations (moderate to high interaction); the work is discussed in Section (c).

Experiments are being conducted in a radial flow generator in order to quantify thermal and velocity wake nonuniformity effects on generator performance. The main goal is to establish experimental reduction formulae for this type of nonuniformity, and consequently assess the validity of the numerical 2-D wake models. The experiment pioneers efforts to create non-uniformities which are well-defined and controlled. Photographs of the generator walls are shown in Figure 3; the assembly is illustrated in Figure 4. Up to fifty-one electric field measurements can be made in and out of the wake zones. Two quartz windows (1-1/2 inch diameter) are located on the plexiglas walls for interferometric measurements.

Instrumentation ports for optical diagnostics (e.g., electron density measurements) and for stagnation and static pressure measurements are

distributed along the disk. The downstream wall is convergent with an angle of $3^{\circ} 30'$ in order to maintain a nearly constant conductivity; the ideal conductivity calculated is between 8 mho/m and 50 mho/m (depending on test gas and operating conditions). Aerodynamic profiles initially in the subsonic portion of the flow (see Figure 4) will be used to create the wakes. The trailing edge will be located at the sonic point ($M = 1$ in the flow). The shape and number of profiles will be determined by the extent of the conductivity deficit calculated at the trailing edge (which leads to an initial inlet wake profile), coupled with the 2-D wake analysis for the open-circuit voltage reduction. This is discussed further below.

Two cases will be analyzed: (1) moderate to high interaction (without shocks), and (2) weak interaction. In the first case, the flow (outside of the wake region) is characterized by a high electron density level ($N_e \sim 10^{20} \text{ m}^{-3}$) with Saha equilibrium and negligible electron kinetics. Within the wake region kinetics effects may be significant, particularly near the inlet of the generator (or for narrow wakes with large temperature gradients). Local electron density and E-field measurements as well as the open-circuit voltage will be compared with the coupled 2-D numerical wake model, where the inlet conditions are specified.

For the weak interaction case, the N_e level low and the flow is characterized by slow electron kinetics (i.e., the characteristic time for electron ionization/recombination is long relative to the flow time in the generator), and the electron behavior in the wake is not affected by the MHD interaction; the nature of the wake is determined by the boundary layer on the cold strut/aerodynamic profile. This case will be analyzed using the uncoupled 2-D model (solving the electrodynamics only) with specified profiles (both radial and azimuthal) of velocity, conductivity and Hall

parameter. For both cases, the Hall parameter will be in the range $3 \leq \beta_{\text{eff}} \leq 5$. An inherent advantage of the slow kinetics case is the suppression of ionizational nonuniformities (which can occur even in molecular gases [8]). This result is evidenced in the experiments discussed below.

For each case the experiments are carried out in two phases. First, the reference or undisturbed flow must be determined/characterized and compared with 1-D calculations which include the effects of finite-rate electron kinetics (primarily for the second case) and stability with respect to ionization. The reference flow should be as free as possible from background nonuniformities. Once this is accomplished, experiments with wakes will be made. The first phase is near completion. The work described below addresses the (i) 1-D model used to analyze the reference flow, (ii) experiments and analyses with several test gases, and (iii) aerodynamic profile design calculations.

(i) 1-D reference flow model-

Previous experiments and analyses of molecular gas tests in small-scale supersonic (shock-tube driven) disk generators have illustrated the important role of finite-rate chemical kinetics on channel design [4,6,8]; flow times are typically of the order of ~ 50 μsec . Moreover, molecular gases are not exempt from ionizational nonuniformities, particularly for diatomic molecules where the inelastic collision loss factor is less than $\delta \sim 500$. [5,8] These effects, therefore, were included in the 1-D model used to design the reference flow conditions for the wake study.

A two-temperature quasi 1-D model originally developed at MIT for non-equilibrium (thermal) generator studies was modified for use with molecular

gases. The basic model, which includes finite-rate electron kinetics and energetics, is described in detail in section II of Appendix A, which is a paper to be presented at the 18th Symposium on Eng. Asp. of MHD (Butte, June 1979). The modifications (relative to Appendix A) are as follows:

- 1) the number of molecular gas species was extended to three.
- 2) the variation of the collisional loss factor δ with electron density was included in the expression for the critical Hall parameter (stability criterion)
- 3) a temperature-dependent cesium crosssection was added.
- 4) a generalized three-body recombination rate constant, K_{rH} , for electron-cesium ion recombination with heavy particles as the third body was included, i.e.

$$K_{rH} = 6.6 \times 10^{-41} \frac{\delta_H Q_{eH}}{M_H T^{2.5}} \left(\frac{m}{\text{sec}} \right)^6$$

where δ_H , Q_{eH} , m_H are the loss factor, crosssection, and mass of the heavy particle and T is the electron (T_{gas}) temperature. The rate constant is based on an extension of the classical J.J. Thompson theory and accounts for collisional losses during a thermalizing electron-neutral collision in the vicinity (sphere of influence) of an ion. [16]

(ii) Experiments and analysis-

The generator shown in Figure 4 was designed and constructed prior to the availability of the 1-D model described above in (i) above. Its design was based on a simple one-temperature fluid code with the assumptions of full thermodynamic equilibrium and a uniform plasma. Initial reference flow tests in N_2 with this configuration were conducted (in Nov. '78) for stagnation conditions in the range $2900 \leq T_0 \leq 3900$ K, and $3.0 \leq p_0 \leq 7.0$ atm for $1.6 \leq B \leq 3.0$ Tesla. The test time was nominally 0.8 msec and the average Mach number ≈ 1.7 .

The results of this first series of tests indicated some difficulty with respect to the reference flow uniformity; the average effective Hall

parameter was in general lower than the ideal value along the channel. Measurements of the azimuthal voltage indicated significant spatial non-uniformity in some cases, and the corresponding plasma luminosity photos revealed the existence of spokes along channel (for these cases --see Figure 6).

With the aim of obtaining a more uniform reference plasma, two modifications of the generator were made. To avoid a possible flow separation in the expansion region the nozzle was redesigned, as shown in Figure 5. It is made of plexiglass in order to observe the inlet flow luminosity, and is instrumented for detailed inlet flow voltage measurements. The central back wall support rod/sting was removed (Figure 5), and replaced with an external support mechanism. Subsequent tests and calculations for N_2 indicated that the main source of the nonuniformity was related to nonuniform seed distribution as a result of shock wave attenuation and bifurcation effects in the shock tube stagnation region in N_2 (for these operating conditions). Consequently, other test gas mixtures were considered.

For the experiments described below, argon was used as the buffer gas (in place of N_2) for mixtures of Ar/CO and Ar/CO₂. For a given stagnation temperature, the higher specific heat ratio of argon leads to a longer and more uniform initial slug of stagnation gas in the shock tube, and a longer test time (relative to N_2 /CO₂ mixtures); the average molecular weight is higher and the number of vibrational modes are less for the Ar mixtures.

Ar Tests

Experiments in pure Ar were conducted (in the channel shown in Figure 5) prior to tests with Ar-molecular gas mixtures. These experiments afforded the maximum test time (up to 3.5 msec) and provided an assessment

of the validity of the two-temperature model under strong nonequilibrium conditions. In parallel with these tests, the modifications to the model, described in (i) above, were made, and methods to improve the signal to noise (due to reflected light) ratio for P-M tube measurements of the electron density in molecular gases were examined (discussed further below).

The conditions for the experiments were selected to produce a stable plasma (i.e., $\beta_i < \beta_{crit}$). This enabled an assessment of both the reference flow uniformity and the validity of the model without regard to particular reduction formulae calculations. The first series of tests were conducted at the following conditions: $T_0 = 2920$ K, $P_0 = 8.5$ atm, $B = 0.9$ T, seed fraction = .07%. The seed fraction is low and, therefore, the cesium becomes fully ionized near the generator inlet. This eliminates the effects of ionizational nonuniformities. The second series of tests were conducted at a high seed fraction and low stagnation temperature to obtain results when the seed is not fully ionized. Those conditions were: $T_0 = 2275$ K, $P_0 = 10$ atm, $B = 1.35$ T, seed fraction = .4%. The test conditions were also chosen to insure that MHD interaction-induced shocks were not present in the generator and static pressure measurements were made to monitor the level of interaction. The average Mach number in argon for low interaction was 1.8. All experiments were conducted at open circuit so the effective Hall parameter could be determined from the voltage profile.

The results of the high temperature tests with low seed fraction are summarized in Figures 7 thru 11. The low seed fraction insured that the ideal Hall parameter was less than the critical value throughout the generator (Figure 7), and resulted in fully ionized cesium at a radius of approximately 10. cm. This is evident from the small temporal variation of

electron density shown in Figure 8. The azimuthal rms variation in voltage of approximately 4% seems to be moderate since no significant reduction in the Hall parameter was observed. Also, the flow appeared to be uniform as shown in Figure 9. Figure 9 is a reprint from a high-speed movie frame. The camera was viewing a portion of the generator test section and the shock tube. The experimental electron density and voltage profiles are compared with those predicted by the model in Figures 10 and 11, respectively. Agreement is good in both cases. The full ionization of the seed is apparent in the electron density profile as the level drops near the exit, since it now follows the gas pressure. This effect is underestimated by the model, possibly due to a small difference in interaction related to weak background nonuniformities.

The results for the low temperature tests are summarized in Figures 12 thru 20. The experimental electron density results in Figure 12 show that the inlet electron density is approximately 10^{20} m^{-3} . If no MHD interaction or radiative heat transfer takes place upstream of the throat and with the plasma in equilibrium in the stagnation region, the initial electron density would be approximately 10^{19} m^{-3} . The initial electron density for the theoretical calculations in Figure 12 was chosen to obtain the value measured near the generator inlet at 7.7 cm. The electron density and voltage profiles labeled curve 1 of Figures 12 and 13 are results of a full calculation by the model. The Hall parameter profiles are shown in Figure 14 which indicate the plasma is stable for $R > 7.3 \text{ cm}$. Curves 2 of Figures 12 and 13 are results of a semi-empirical calculation using the experimentally determined effective Hall parameter and the assumption $\sigma_{\text{eff}}/\sigma = \beta_{\text{eff}}/\beta$. The Hall parameter profiles for this case are shown in Figure 15. Inspection of Figure 15 reveals that the electron

density plateau observed experimentally (Figure 12) at $R = 10.5$ cm coincides with a strong reduction in the effective Hall parameter ($\beta_e/\beta_i = .206$). Also, the plasma is predicted to be stable beyond $R = 9.3$ cm. Nonuniformities persist throughout the generator, however, and are indicated in Figures 16 and 17. Figure 16 is an electron density history measured at a radius of 13.5 cm, and Figure 17 is from a high-speed movie frame. Spokes are evident in the luminosity photograph. The model, which considers only ionizational nonuniformities in this case, is not able to predict the effective plasma properties. This implies that the formulation used to determine these properties is not applicable and the nonuniformities need to be better characterized.

The information related to the nonuniformities in the low stagnation temperature experiments is contained primarily in the electron density measurements shown in Figures 18 thru 20. Figure 18 compares the signal from two photomultiplier tubes located approximately 35° apart at a radius of 13.5 cm. The signals are very similar and there is little phase shift between them. A third signal obtained at a radius of 13.5 cm and between the two in Figure 18 gave similar results. Figure 19 is a comparison between signals obtained at different radial locations, 5.8 cm apart and along the same azimuth. The similarity is not as strong as in the azimuthal direction and a phase shift of 45-50 μ sec has been measured. The average frequency over several tests was 10 kHz, and the average rms of fluctuations is 40%. Figure 20 is a plot of the rms fluctuations. Note that the growth and decay of the fluctuations reflect the pattern of the effective Hall parameter in Figure 15. From these measurements it is difficult to define the structure of the nonuniformity. The uniformity of the electron density in the azimuthal direction (Figure 18) as well as the

phase shift occurring in the radial direction (Figure 19) suggest a radial nonuniformity. If this were the case and the wavelength were greater than the distance between probes, the velocity of the wave would be ~ 1300 m/s and its wavelength ~ 13 cm. Additional experiments need to be made with a streak camera, however, to better define the wave structure.

Theoretically, various types of waves can exist under the plasma conditions (i.e., elevated electron temperature and low Hall parameter (~ 1.0)) for the low temperature tests. Magnetoacoustic, electrothermal (ionization), and thermal waves were studied by Hougen and McCune^[17], who included the coupling between electrons and heavy particles. Based on their results, the following possibilities exist. Magnetoacoustic waves could propagate either antiparallel to the azimuthal current (spokes) or against the flow, depending upon the type of initial disturbance. These waves are not, however, detected by the pressure measurements and they are not likely to exist at the wavelengths (10 cm) required for their phase velocity (~ 650 m/s) and direction of propagation ($\pi/2 < \theta < \pi$; θ measured from the azimuthal current) to be compatible with the frequency of the disturbance (10 kHz). Ionization waves would be convected with the flow, and oriented at approximately $\theta = \pi/4$. As discussed above, however, growth occurs when the Hall parameter is greater than the critical value for the instability and this condition is only satisfied at the generator's entrance (Figure 15). However, the stability criterion could be altered by the presence of other waves or nonuniformities (Figure 17). Finally, a thermal wave could exist which would also be convected with the flow and oriented at $0 < \theta < \pi$. The growth rates are large, even at low Hall parameters, when the electron temperature is elevated. The wavelengths of the thermal waves are consistent with the flow velocity, orientation and observed frequency.

Considering the large angle of orientation over which these waves exhibit growth, a radial disturbance (i.e., from the shock tube) could initiate a thermal wave. Table II is a summary of the conditions described above.

Despite the discrepancies encountered in the low stagnation temperature tests, two encouraging points emerge: (1) the model was able to reproduce quite well the experimental electron density profile when supplied with the measured effective Hall parameter and (2) high levels of nonequilibrium ionization ($\sim 2 \times 10^{20} \text{ m}^{-3}$) were obtained despite the background nonuniformities present in the generator. Additional work is required in order to establish a more uniform reference plasma before better agreement between calculated and measured effective Hall parameters can be obtained for these conditions. Further details of these experiments are given in Appendix A.

Ar/CO Tests

Tests were conducted with a 70% Ar - 30% CO mixture for two stagnation conditions and several B-field strengths. The mixture and conditions were chosen to provide adequate test time and to allow an assessment of the stability of the plasma. Unlike the pure Ar tests, the regime where $\beta_i > \beta_{cr}$ was examined. An advantage of using CO (rather than CO_2) is that it can be treated as chemically inert; no dissociation occurs at stagnation temperatures of interest. However, the inelastic collision loss factor ($\delta \approx 200-500$) is appreciably less than that of CO_2 ($\delta \approx 2500$), resulting in a correspondingly lower critical Hall parameter.

The first series of tests were conducted at the following conditions: $T_0 = 2800 \text{ K}$, $p_0 = 5.7 \text{ atm}$, seed fraction = .16% C_s and $B = 1.0, 1.6, 2.0$ and 2.4 Tesla, at open circuit. The results for $B = 2.0\text{T}$ are summarized in

Figures 21 thru 27; the profiles for the other B-fields illustrate similar behavior. Again, the curves labelled (1) are results of a full calculation by the model (with $\delta_{CO} \approx 230$), and the curves (2) are results of a semi-empirical calculation using the experimentally determined effective Hall parameter. The effective Hall parameter profiles used to calculate curves (1) and (2) are shown in Figures 22 and 23, respectively. Figure 22 (for the full calculation) indicates that the plasma is unstable throughout the generator. The measured β_{eff} , however, is substantially higher than the critical value, β_{cr} , as seen in Figure 23. For this operating condition, the plasma is actually stable as a result of the slow electron kinetics. Ionizational instabilities/nonuniformities, which were assumed to develop instantaneously in the modeling, are suppressed. This can be seen in Figure 26 where the characteristic ionization time (for a 40% Ne increase), τ , relative to the flow time, τ_{fl} , is shown as a function of radius; for most of the generator, $\tau > \tau_{fl}$ and the plasma simply can not respond to disturbances, e.g. in temperature. Luminosity photos indicate that the flow is uniform for these tests; an example is shown in Figure 27. The slight decrease in β_{eff} in Figure 23 is consistent with the pressure increase in Figure 24. That is, if the experimental pressure were used to determine β_i (in Figure 23), then β_i and β_{eff} would be in closer agreement. The reason for this slight discrepancy between the calculated and measured pressure profile is not clear, and may be related to the abrupt change in angle of the downstream wall at $R = 8.8$ cm (radius).

The voltage profiles shown in Figure 21 and the electron density profiles in Figure 25 for curves (1) and (2) are substantially different, and the differences between the two curves are a direct effect of the different β_{eff} 's used in the calculations. Uncertainties in seed fraction

or loss factor, δ (see Figures 40 and 41), cannot make up the large differences between the two calculations. The seed fraction is determined to within a few percent of the nominal value by continuum radiation measurements behind the reflected shock in the stagnation region (see Appendix A). The dashed curves in Figure 25 represent the electron density for Saha equilibrium at the electron temperature, and the differences between corresponding dashed and solid curves illustrate the extent of the electron kinetic effects.

Measurements of the electron density for these tests were not possible with the present P-M tube light pipe adapters. The generator luminosity is nearly four orders of magnitude lower than the light emitted in the stagnation region (i.e. $Ne/Ne_0 = 10^2$). Consequently, light reflections from the stagnation region were a severe problem. New adapters have been designed which contain six baffles for low angle reflections and thirty baffles for high angle reflections. These adapters will be tested in the near future.

Figure 28 summarizes the results for the Hall parameter saturation for these tests. Here the inlet and exit measured and calculated effective Hall parameters are compared to the ideal values (for the four B-field cases). At the inlet, no saturation occurs for the experiment (kinetically-limited) while the full-calculation predicts saturation at about $\beta_{eff} = 2.0$. At the exit the measured β_{eff} appears to be limited to about 4.2. Figure 29 shows the voltage history at three locations in the generator for the highest B-field case ($B = 2.44T$). The voltage is seen to be nearly uniform during the test time (≈ 1.4 msec), with some unsteadiness/fluctuations appearing with increasing distance. The characteristic ionization time decreases with B-field, as seen in Figure 30, and strongly with stagnation temperature (or Ne level in the generator).

The test conditions described above appear to be adequate for the wake study for the weak interaction case where the electron plasma is kinetically limited and the electron density level is low. The transition from the parallel-wall segment of the nozzle ($R < 8.8\text{cm}$) to the converging channel wall, however, should be more gradual. In addition, the initial wake width and conductivity deficit must be assessed. For this case the slow kinetics may require a large number of struts/profiles (or special design considerations such as boundary layer tripping) in order to provide a measureable effect on performance. Profile design is addressed in (iii) below.

A series of tests were conducted at higher stagnation temperature in order to observe the effect of a faster plasma response (lower τ/τ_{fl} , as indicated in Figure 30) on the Hall parameter saturation, or stability of the plasma with respect to ionizational nonuniformities. The test conditions were: $T_0 = 3700\text{ K}$, $p_0 = 6.5\text{ atm}$, seed fraction = .043% Cs and $B = 1.6, 2.0$ and 3.5 Tesla , at open circuit. The same mixture 70% Ar-30% CO was used. A detailed discussion of the results which are similar/common to the low temperature case is not repeated here; the important differences are addressed.

The results for $B = 1.6$ and 2.0 T are qualitatively similar to the low temperature results and no saturation occurs near the generator inlet (Figure 37a). However at $B = 3.5\text{ T}$, voltage fluctuations appear in the generator, and the inlet β_{eff} is less than β_i . Figures 31 thru 36 give the radial profiles for the $B = 3.5\text{ T}$ case; β_{eff} is shown relative to β_i in Figure 37. A comparison of Figures 38 and 39 show the extent of the voltage fluctuations at 3.5 T relative to the quasi-steady results at lower B-fields (1.6 T). The electron kinetics are about five times faster at

$B = 3.5T$ than at $B = 1.6 T$ for these conditions, and more than an order of magnitude faster than the corresponding low temperature ($T_0 = 2800 K$) conditions (see Figure 30). This increase in plasma response (or reduction in ionization time,

τ/τ_{f1}), however, is not sufficient to provide agreement with the full calculation based on stability criterion for infinite rate electron kinetics.

With respect to the radial profiles in Figures 31 thru 36, a peculiar behavior of the semi-empirical result occurs near $R = 11.0cm$. This behavior may stem from an aerodynamic (or flow) nonuniformity caused by the 3.5° angle change in the downstream wall at $R = 8.8cm$. A similar Hall parameter reduction was found in the pure Ar tests (see Figure 15). The wall contour is currently being modified (see below) to eliminate this abrupt change in angle.

The electron vibrational energy loss factor δ_v (approximately equal to the total energy loss factor δ) is strongly dependent upon electron temperature and distribution function. Under MHD conditions, the electron distribution is near Maxwellian; for CO at temperatures in the range of $2000 \leq T \leq 3000$, δ varies between $200 \leq \delta \leq 500$ (approximately).^[18] Figures 40 and 41 (which correspond to Figures 31 and 33, respectively) show calculations for $\delta_{CO} = 500$. It is clear that although better agreement is found at the higher δ , the differences between the full and semi-empirical results can not be explained by variations in the loss factor; the slow electron kinetics remain the primary cause of the discrepancy.

Calculations of the electron density using the model were made at short circuit ($I_{s.c.} = 65$ amps) and compared with the experimentally determined^[4] Ne for several cases. Figure 42 an example of the good agreement that was found.

Ar/CO₂ Tests

Several tests were conducted with a 80% Ar-20% CO₂ mixture (initial composition at T = 300 K) for B = 2.0 and 2.6 Tesla. The stagnation conditions were: T₀ = 2900 K, p₀ = 6.5 atm with .16% Cs, yielding an equilibrium composition of 80% Ar-13.4% CO₂ - 6.6% CO behind the reflected shock^[19]. Chemical kinetics calculations for the neutral species show that the composition remains nearly frozen during the expansion^[4], and consequently, the 1-D model could be used (which accounts for electron-kinetics only -see appendix A).

Because of the high loss factor for CO₂ ($\delta = 2500$) the critical Hall parameter obtained is higher than with CO alone. Figures 43 thru 46 show the profiles obtained for the B = 2.6 T case, and Figure 47 summarizes the Hall parameter results. The voltage profile in Figure 43 is in close agreement with the results of the full calculation, and therefore no semi-empirical calculations were necessary. Saturation of the effective Hall parameter, as shown in Figure 47, was predicted quite well by the model. The electron kinetics at 2.6 T are more than a factor of six faster than for the 3.5 T, high temperature CO case, i.e., from Figure 46, $\tau_{\max}/\tau_{fl} = .062$ versus .4 from Figure 30. Figure 45 shows that the electron density is in Saha equilibrium.

New Channel Design

Modifications of the channel are currently underway. Figure 48 is a scale drawing in which the two configurations are compared. The "new channel" has been designed (with the model in (i) above) primarily for the moderate interaction case where kinetics effects are small and Saha equilibrium for the electrons is maintained. The modifications are summarized in

the following table:

	Old Design	New Channel
Inlet turning radius (cm)	.762	1.854
throat area, A^* (cm^2)	65	58
Radius of throat, R , (cm)	6.6	7.7
Angle of wall, α ($^\circ$)	0° for $R < 8.8$ cm 3.5° for $R > 8.8$ cm	3.5°
Channel height at $R = 8.8$ cm (cm)	1.554	1.140
Mach No. at $R = 8.8$ cm (for Ar/CO_2)	1.65	1.25

The change in wall angle at $R = 8.8\text{cm}$ has been eliminated, and the throat area has been reduced to provide approximately a 10% increase in test time. The Mach number has been reduced to increase the channel conductivity for given stagnation conditions.

The base case for the design are the Ar/CO_2 conditions discussed above. Figures 49 thru 51 show profiles of the Hall parameter, electron density, and the τ/τ_{f1} ratio. At $B = 2.6$ T the Hall parameter is about four and does not saturate (saturation occurs at $\beta \approx 5.2$ at $B = 3.5$ T). N_e is nearly constant and nominally $1.8 \times 10^{20} \text{ m}^{-3}$, with $\langle \sigma \rangle \approx 45 \text{ mho/m}$, and $\tau/\tau_{f1} < .025$. Reference flow tests with this modified generator will begin soon.

(iii) Aerodynamic profile/strut design-

The shape and number of aerodynamic profiles or struts that will be

initially used/selected to create the wakes will be determined as follows. First, the boundary layer profiles (see below) will be calculated at the trailing edge for several configurations of slender struts, and the initial wake width and wake profiles will be estimated. The 2-D model of Dvore, et.al.^[12] will then be used to determine the effects of the wakes on the generator performance. In particular, a predicted open circuit voltage reduction of roughly 50% will be used as a criterion for the initial strut selection. An example of the procedure is given below.

Development of Strut/Aerodynamic Profile Boundary Layers

Because of the small physical scale of the struts used to induce conductivity defects in the generator flow, it was considered imperative that calculations of the strut boundary layers should include the effects of finite-rate electron-recombination. The most readily available program for performing boundary layer calculations appropriate to the disk generator experiments was developed and documented by Daily^[20]. His program calculates boundary layer development on the walls of a duct through which a high temperature gas is flowing. The program was designed for turbulent boundary layers, and it uses the eddy viscosity and mixing length concept for modeling the turbulent transport of momentum energy and mass. Current flows are permitted, so that an MHD body force appears in the momentum equation and a Joule heating term in the energy equation. The equation for electron-ion continuity includes a source (or sink) term to account for the loss of electrons by recombination, with 3-body reaction processes for both electrons and heavy particles serving as the 3rd body.

The free-stream boundary condition is applied at the duct center-line, so that mass conservation is maintained by integrating over the entire half

width of the channel. This is not strictly applicable to the disk generator experiment, where the strut boundary layers are more accurately modeled as flat-plate cases with axial pressure gradients imposed. However, since the duct formulation does permit inclusion of an axial pressure gradient, it was felt that the program would give results indicative of the boundary layer growth on the disk generator struts.

Gas properties for a mixture of 80% Ar, 20% CO₂ with 0.16% Cesium seed were initially calculated, with inclusion of the dissociation of the CO₂ as required by thermodynamic equilibrium. Initial calculations for representative conditions indicated that the momentum Reynolds numbers for the boundary layers were much smaller than would prevail in a turbulent boundary layer. Accordingly the program was slightly modified, with the initial profile being calculated for the laminar case, and with the turbulent growth parameters initially suppressed; only when the developing laminar boundary layer had grown to the point where $Re_m > Re_{m_{crit}}$ were the turbulent transport mechanisms allowed to operate. Calculations were made with $Re_{m_{crit}} = 360$, appropriate for a smooth flat plate (see below).

Example Results

Calculations were made for the boundary layer development on slender (flat-plate) struts for two cases; (1) short struts ($l = 1.7$ cm chord) where the layer is laminar and the trailing edge $M_{T.E.} = 1$, and (2) long struts ($l = 7.7$ cm chord) where transition occurs (at approx. $x/l = .78$) and $M_{T.E.} = 1$. Slender profiles are desired from the standpoint of creating conductivity wakes; later phases of the study may include the effects of moderate momentum wake deficits. The input pressure gradient dp/dx was determined from 1-D calculations. Rate constants for three-body electron recombination correspond to those used in the 1-D two-temperature

model, i.e. $1.09 \times 10^{-20}/T^{4.5}(\text{m}^6/\text{sec})$ for electrons as the third body, and the rate given in section (i) above for heavies as the third body.

Electron density and conductivity profiles in the trailing edge boundary layer are shown in Figures 52 thru 55. Values are normalized to the free stream conditions (∞) and the boundary layer thickness (δ); δ is defined where $u/u_\infty = .99$. The dashed curve in Figures 52 and 53 are values which would be obtained if the electrons were in Saha equilibrium, and illustrate the strong kinetics effects in the layer. The normalized profiles for the two cases, (1) and (2), differ by about a factor of 1.6 in the middle of the layer, and are about the same near the wall (i.e., for $y/\delta = .1$). The major influence as far as the near wake is concerned (other than the fact that (1) is laminar and (2) is turbulent) is the initial wake width (approximately 2δ). For the laminar case, $2\delta = .34$ mm and for the turbulent case, $2\delta = 1.27$ mm. The reduction in open-circuit voltage (see Figure 2) depends strongly on rms σ , which for the same σ_w/σ_∞ , obviously decreases with narrower wakes. The long strut case will most-likely be used for the first series of tests with struts (in the new channel), and will consist of a "spider" of flat-plate profiles (up to 50) approximately of 0.38 mm thickness (stainless steel).

In order to estimate the effect of the wake nonuniformity created by these profiles on the open-circuit voltage reduction, calculations were first made with the (readily available) uncoupled 2-D wake model of Dvore^[12]. Since the model is applicable only for weak interactions, the stagnation temperature chosen for the example was $T_0 = 2500$ K (reduced somewhat from the design base case where $T_0 = 2900$ K), and $B \leq 2.0$ Tesla was used. Radial profiles of the fluid properties were input from the 1-D calculations, as well as the Hall parameter $\beta(r)$. Although the program

allows for σ , β , and u to vary with angle θ (azimuthal direction), only conductivity wakes were considered in these initial calculations.

The model was slightly modified (before carrying out the calculations) to account for variable wake growth and conductivity decay parameters. A shortcoming of the uncoupled problem is that the development of the wake is not calculated and must be input to the program. Although MHD effects are not present, the conductivity wake development in an axisymmetric flow with electron kinetics is not well understood/modeled. The wake growth and σ_w (wake centerline value) were modeled as:

$w(r)$ = wake width

$$= w_1 \left[1 + \alpha \left(\frac{r-r_1}{r_2-r_1} \right)^\epsilon \right]$$

and

$$R(r) \equiv \frac{\sigma_w}{\sigma_\infty} = R_1 + (R_2 - R_1) \left(\frac{r-r_1}{r_2-r_1} \right)^\gamma$$

where the subscript 1 refers to the inlet or initial value at r_1 and subscript 2 corresponds to the exit value at r_2 ; the additional inputs therefore are w_1 , α , ϵ , R_1 , R_2 , and γ . The wake profile (at any radial position, r) is fitted by a cosine curve thru σ_w and σ_∞ . That is,

$$\sigma(r, \theta) = \begin{cases} \frac{1}{2} [\sigma_\infty(r) + \sigma_w(r)] + \frac{1}{2} [\sigma_\infty(r) - \sigma_w(r)] \cos \frac{2\pi r}{w(r)} \left[\theta - \left(\frac{\pi}{N} - \frac{w(r)}{2r} \right) \right] \\ \text{for } \frac{\pi}{N} - \frac{w}{2r} < \theta < \frac{\pi}{N} + \frac{w}{2r} \\ \frac{1}{2} [\sigma_\infty(r) + \sigma_w(r)], \text{ elsewhere} \end{cases}$$

where N is the number of struts/wakes. The formulation can be applied to the velocity and Hall parameter as well by replacing σ with either u or β .

Calculations were made for $\alpha = 1.4$; $\epsilon = .5$, $R_1 = R_2 = .2$ for $N = 25, 50$ at $B = 1, 2$ Tesla. ($T_0 = 2500$ K, $p_0 = 6.0$ atm, .16% Cs, $u = u(r)$, $\beta = \beta(r)$), for the long strut case, i.e. $w_1 = 2\delta = 1.27$ mm. The value of $R_1 = .2$ corresponds to the trailing edge boundary layer conductivity at $y/\delta \approx .4$; this is felt to be on the conservative side, with the possibility of $R_1 = .1$ at $y/\delta \approx .1$ being more appropriate/realistic. However, in the near wake, properties change more rapidly (due to the discontinuity in center-line boundary conditions from flow along a wall to wake flow); hence, $R_1 = .2$ was used for the first set of calculations. The rms σ in mho/m is calculated from

$$\text{rms } \sigma \text{ (mho/m)} = \sqrt{w_1^1 (1.5 - w_1^1)} \left(\frac{\sigma_\infty - \sigma_w}{2} \right)$$

and the average conductivity is

$$\langle \sigma \rangle \text{ (mho/m)} = \frac{\sigma_\infty + \sigma_w}{2} + (1 - w_1^1) \frac{\sigma_\infty - \sigma_w}{2}$$

where $w_1^1 = wN/2\pi r$. The results of the calculations are summarized in Table III. The values of rms σ are normalized by $\langle \sigma \rangle$ in the table. $V_{o.c.}$ is the open circuit voltage of the undisturbed/uniform 1-D flow. The results clearly indicate the potential of obtaining a 50% voltage reduction for the Hall parameters which can be achieved in the experiment. Calculations for the experimental conditions using the fully coupled 2-D model^[12] are currently underway.

(b) Instrumentation and Diagnostic Development

(i) Data acquisition system-

High-bandwidth A/D (analogue to digital) equipment was delivered and installed at the disk facility for preliminary checkout during the latter part of 1978. This equipment consists of 3 Lecroy A/D digitizers allowing data sampling to 2 MHz, associated Kinetic Systems Camac-standard support equipment, and 16 rack-mounted amplifiers to buffer incoming signals to the digitizers.

The equipment was increasingly integrated into the experimental program during the last six months. The Lecroy digitizers were used in the local mode, at first to back up existing instrumentation, and subsequently to record up to 24 channels of data per run. The playback features of the digitizers allow photographic storage of data superior to existing oscillographic techniques, since display time and amplitude content can be chosen specifically.

The establishment of communication links to the Sloan Laboratory PDP 11/60 computer in Bldg. 31 was initiated concurrently. Development of software to handle data transfers via the Kinetic Systems micro-processor based communications interface was initiated to allow data storage at the computer control center for later analysis.

Applications to date have included high frequency (200.KHz) acquisition of voltage and electron density fluctuations enabling a more complete analysis of the plasma nonuniformity structure to be performed through the use of cross correlation routines. For a large scale ($>1\text{cm}$) well defined nonuniformity this type of analysis has resulted in the determination of the orientation and propagation velocity of the nonuniformity (see Appendix A). In addition to cross-correlation, a subroutine library contains

programs to generate histograms, power spectrums, and perform peak processing. These types of signal processing will be required in the study of nonuniformities, particularly when they are of small scale ($<1\text{cm}$).

(ii) Diagnostics-

During this reporting period we have implemented several improvements into the laser interferometer system. It was determined that considerable RF noise was being produced by the ruby laser. To remedy this situation, the main laser power supply and laser head were shielded. AC line isolators were installed so that no transient RF pulse could leak out of the laser system.

The metallic optical mounts for the interferometer were affected by the magnetic field resulting in fringe distortion. The problem was corrected with non-metallic mounts which were designed and manufactured at MIT.

Based on past experiments there arose a need for increasing the sensitivity of the interferometer, particularly for use with the nonuniformities study. A technique which involves diffraction and interference theories is outlined:

A normal interferogram is produced by superimposing two wavefronts U_1 and U_2

$$U_1 = a_1 e^{i k [\phi(x,y) + x \sin \theta_1 + \delta_1]}$$

$$U_2 = a_2 e^{i k [x \sin \theta_2 + \delta_2]}$$

$$I = |U_1 + U_2|^2 = a_1^2 + a_2^2 + 2 a_1 a_2 \cos\{k[\phi(x,y) + x \sin \theta_0 + \delta_1 - \delta_2]\}$$

where $\delta_1 - \delta_2$ is the optical path difference

and $\sin \theta_0 = \sin \theta_1 - \sin \theta_2$ (the sines of the angles of the wavefronts to the normal) and ϕ is the phase disturbance or density change.

If after processing this interferogram (which contains the gas density information), we replace it to its original position in the interferometer and examine the resulting diffraction pattern we can then expect, at normal incidence, two wavefronts

$$U_{-N} = b_{-N} e^{i k [N\phi(x,y) - N(\delta_1 - \delta_2) + \Delta_1]}$$

$$U_N = b_N e^{i k [N\phi(x,y) + N(\delta_1 - \delta_2) + \Delta_2]}$$

$$b_{-N} = b_N = b \quad \text{and} \quad \delta_1 - \delta_2 = \Delta_1 - \Delta_2 \sim 0.$$

The intensity variation observed is

$$|U_{-N} + U_N|^2 \sim 2b^2 [1 + \cos 2kN\phi(x,y)].$$

Therefore by separating the diffracted orders and then selectively recombining the pairs, the original interferogram is amplified N times, N being the order difference. This procedure (discussed more fully in reference 21) will be investigated in the near future.

(c) Two Dimensional Modeling of Open-Cycle Disk Generators

In this reporting period, we initiated a comprehensive theoretical investigation to be used for both small and large scale disk generator design and performance predictions. These investigations will be carried out in several stages, requiring both modifications of present codes and development of new codes. We divide our approach in two major sub-tasks:

(i) Effects of wake nonuniformities (r, θ plane)

(ii) Effects of boundary layer, slag, and breakdown (r, z plane).

In the following, these stages are outlined, and the governing equations and progress to date are presented.

(i) Effects of Wake Nonuniformities

The beneficial effects of inlet swirl in disk generators have been noted before^[1], and have led to concepts where the flow swirl is introduced by inlet guide vanes. An undesirable side effect of these vanes is the production of wakes, both thermal and dynamical, and it is of interest to analyze the extent to which these wakes can impair the performance and cut into the gains inherent in the swirl.

Previously^[12], we presented results of two dimensional electrical flow calculations in the plane of the disk. In the first part of the work, the flow was considered unaffected by the MHD effects (small interaction limit) and the purely electrodynamic effects of the wakes were analyzed. In the second part a significant interaction level is introduced, and the electrodynamic code was coupled with a simple two-dimensional fluid code which provides velocity, temperature and pressure data over the computational region. Some results were given that show the dynamics of inviscid wakes. The interaction tends to stabilize thermal wakes and to

steepen velocity wakes^[12]. However, the method of calculation for the fluid dynamics part of the problem (which is restricted to purely supersonic flows without shocks), has some numerical stability limitations as shown in Figure 56 (due to the hyperbolic nature of the governing system of equations), and its convergence is found to be limited to a narrow range of cases.

In this reporting period, recent modifications and results from the electrodynamics (uncoupled) model were obtained and reported above in section (a).

Currently, we are modifying the fluid dynamics code, in (r, θ) plane, by using a multi-point finite difference technique in order to increase the stability and accuracy, and to facilitate the introduction of the thermal conduction and viscous transport terms. Also, we are investigating the use of small perturbation techniques (linearized theory) to study the effects of steady state periodic inlet disturbances (nonuniformities) in the azimuthal direction. The linearized fluid dynamics equations and the corresponding electrodynamic equations are given in Appendix B.

(ii) Effects of Boundary Layer, Slag, and Breakdown

The most critical electrical constraint which affects the performance/scaling of the disk is the maximum Hall (axial) field that can be sustained on an insulating wall.

Since the disk has only one electrode pair, the radial electric field along the insulator wall can be significantly larger than the axial field in a linear channel. This leads to corresponding larger power densities in disk generators. However, compared to the linear generator (where the field is limited to $E_{\max} \sim 4 \text{ KV/m}$), relatively little information exists

for $E_{\max}$ in disks. In shock tube experiments^[8], Hall fields up to $E \approx 38$ KV/m were measured with simulated combustion gases; these experiments, however were conducted under ideal wall and flow conditions ($T_{\text{wall}} \approx 300$ K) without slag.

In a disk generator, it is expected that the current distribution in the magnetic field direction will not be uniform due to the variation of the conductivity resulting from a developed thermal boundary layer near the side walls. As the current distribution is altered, it would be expected that the momentum boundary layer would be altered through the $\vec{J} \times \vec{B}$ term, with a consequent influence on the skin friction. Also, the sidewall heat transfer could be affected because of the variation of the Joule heating.

We initiated an analysis to study the boundary layer effects, and the axial field limitation for breakdown on the side walls of disk generators. The slagging effects will be incorporated in the model at a later date.

In the model, the disk walls are assumed to be made of concentric metallic ring frames (modules) separated by ceramic insulators (See Figure 57). The frames are electrically floating and draw no net current, but since they offer a local radial shorting path, eddy current loops are set up whereby the downstream edge becomes an anode. Of course, these currents superimpose on the general azimuthal current, and create additional dissipation quite close to the wall. Under some conditions this may lead to a general overheating of the plasma layer in contact with the wall, and to the establishment of more extensive current loops, which may include several gaps, with current returning through the core (See Figure 58). This is the "shorted" or breakdown condition, and if present, will be highly destructive.

To determine whether shorting occurs for given conditions, and to study the boundary layer effects, an analysis that assumes the steady state conditions will be followed.

Under the conditions of azimuthal symmetry, the governing fluid dynamics equations are:

continuity equation:

$$\frac{1}{r} \frac{\partial}{\partial r} (r \rho U_r) + \frac{\partial}{\partial z} (\rho U_z) = 0 \quad (1)$$

momentum equations:

$$\rho \left[U_r \frac{\partial U_r}{\partial r} + U_z \frac{\partial U_r}{\partial z} - \frac{U_\theta^2}{r} \right] = J_\theta B - \frac{\partial P}{\partial r} - \frac{\partial}{\partial z} [(\mu + \epsilon) \frac{\partial U_r}{\partial z}] \quad (2)$$

$$\rho \left[U_r \frac{\partial U_\theta}{\partial r} + U_z \frac{\partial U_\theta}{\partial z} + \frac{U_r U_\theta}{r} \right] = -J_r B - \frac{\partial}{\partial z} [(\mu + \epsilon) \frac{\partial U_\theta}{\partial z}] \quad (3)$$

$$\rho \left[U_r \frac{\partial U_z}{\partial r} + U_z \frac{\partial U_z}{\partial z} \right] = -\frac{\partial P}{\partial z} \quad (4)$$

energy equation:

$$\rho U_r \frac{\partial}{\partial r} \left[h + \frac{U^2}{2} \right] + \rho U_z \frac{\partial}{\partial z} \left[h + \frac{U^2}{2} \right] = \underline{J} \cdot \underline{E} + \frac{\partial}{\partial z} [(\mu + \epsilon) \frac{\partial}{\partial z} (h + \frac{U^2}{2})] \quad (5)$$

equation of state: $\rho = \rho(P, T)$,

where μ is the dynamic thermal conductivity and ϵ is the turbulent viscosity. The molecular and turbulent Prandtl numbers are assumed to be unity.

The electrodynamic equations can be derived in the form:

$$E = -\nabla \phi,$$

$$J_r = \frac{\sigma}{1+\beta^2} [E_r + U_\theta B + \beta U_r B], \quad E_\theta = 0 \quad (6)$$

$$J_\theta = \frac{\sigma}{1+\beta^2} [-U_r B + \beta E_r + \beta U_\theta B], \quad J_z = \sigma E_z \quad (7)$$

$$\frac{\partial}{\partial r} \left[\frac{\sigma r}{1+\beta} \frac{\partial \phi}{\partial r} \right] + \frac{\partial}{\partial z} \left[\sigma r \frac{\partial \phi}{\partial z} \right] = \frac{\partial}{\partial r} \left[\frac{\sigma r B}{1+\beta} (U_\theta + \beta U_r) \right] \quad (8)$$

The boundary conditions on the electric potential, ϕ , are:

- 1) up and down stream; $\phi(r_2, z) = \phi(r_1, z) + V$
- 2) on the side walls: $\phi = \phi_0$ on the conducting modules, and

$$\frac{\partial \phi}{\partial z} \cos \gamma + \sqrt{1 + \beta^2} \frac{\partial \phi}{\partial r} \sin \gamma = 0, \quad (9)$$

on the insulating gaps where γ is the angle between $\underline{B}$ and normal to the side walls.

We completed developing an iterative numerical code for the electrodynamic part of the problem as stated above, for the case of channels with constant height. Currently, we are testing its accuracy and rate of convergence to obtain a near optimum relaxation parameter.

For channels with variable height, it is convenient to transform the coordinates so that the computational domain would have constant height in the new coordinates.

In such a case one obtains a transformation of coordinates as follows:

$$\xi = r \quad r_1 \leq \xi \leq r_2 \quad (10)$$

$$\eta = z/z_w \quad -1 \leq \xi \leq 1, \quad 1 z_w \leq z \leq z_w \quad (11)$$

where

$$z_w = f(r), \quad r_1 \leq r \leq r_2, \quad (12)$$

is the given wall profile.

Therefore,

$$\frac{\partial}{\partial z} = \frac{1}{z_w} \frac{\partial}{\partial \eta}, \quad \frac{\partial^2}{\partial z^2} = \frac{1}{z_w^2} \frac{\partial^2}{\partial \eta^2}, \quad (13)$$

$$\frac{\partial}{\partial r} = \frac{\partial}{\partial \xi} - \eta \frac{d}{dr} \ln z_w \frac{\partial}{\partial \eta} \quad (14)$$

$$\begin{aligned} \frac{\partial^2}{\partial r^2} = & \frac{\partial^2}{\partial \xi^2} - 2\eta \frac{d}{dr} \ln z_w \frac{\partial^2}{\partial \xi \partial \eta} + \eta^2 \left[\frac{d}{dr} \ln z_w \right]^2 \frac{\partial^2}{\partial \eta^2} \\ & + \eta \left\{ \left[\frac{d}{dr} \ln z_w \right]^2 - \frac{d^2}{dr^2} \ln z_w \right\} \frac{\partial}{\partial \eta} . \end{aligned} \quad (15)$$

We plan to incorporate the above transformation in the electrodynamic code in order to cover the case of channels with variable height. A comparison of results for cases of fixed and variable heights would be made to assess the effects of the variation of the height of the generator.

WORK FORECAST

In the coming months our efforts will be directed primarily towards:

1. Nonuniformities studies-

Reference flow tests for the new channel base case (Ar/CO₂ @ T₀ = 2900 K) will be completed and analyzed. Design and fabrication of the struts will be completed, and the first series of tests with well-defined wakes will begin. Emphasis will be placed on determining the reduction of the open-circuit voltage as a function of the number of wakes. In addition, measurements of the local voltage distribution in the wakes and free stream will be made, as well as the pressure and electron density radial profiles. The results will be analyzed with the 2-D wake models.

2. Studies in a swirl generator-

An assessment of the effects of wakes in a 45° swirl generator will begin. The generator and instrumentation for a quantitative experimental study which parallels/complements the radial flow nonuniformities study (in 1. above) will be designed.

3. Instrumentation and diagnostics-

Application of the high-bandwidth A/D equipment to the characterization of plasma nonuniformities will continue. Signal processing codes will be tested. Sensitivity amplification of the laser interferometer system will be examined in accordance with the technique described in section (b) above. In addition, spectroscopic studies will be planned; of particular concern is the effect (if any) of radiation from CO and CO₂ molecules on the cesium spectrum (in the continuum region where Ne measurements are being made).

4. Disk generator modeling-

The chemical kinetics 1-D design code will be completed. With respect to the 2-D modeling:

- a.) Application of the existing version of the coupled 2-D wake model to the base case test conditions for the study in 1. above.
- b.) Continuation of the small perturbation analysis of wake nonuniformities with high interaction.
- c.) Completion of the electrodynamics code in the meridian (r,z)-plane and studying the effect of the boundary layers and the sectionalization of the side walls for channels with variable height.
- d.) Initiation of modeling the fluid dynamics part in the (r,z)-plane and its coupling with the electrodynamics.

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NONEQUILIBRIUM DISK GENERATOR STUDIES

Appendix A

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Abstract

The nonequilibrium processes in the argon-driven, radial-flow, MHD disk generator are studied. A quasi-one-dimensional, two-temperature fluid model, including the effects of nonuniformities and finite rate kinetics and energetics, is described. Results from the model are discussed in comparison to experimental data. Agreement between theory and experiment is good for the case where the seed is fully ionized; the stabilized plasma exhibited no reduction in the Hall parameter. For conditions where full ionization did not occur, good agreement was obtained when the measured effective Hall parameter was used in the model. This indicates that the actual plasma nonuniformities differed from the ionizational nonuniformities assumed in the model. Local measurements of electron density fluctuations (40% rms, averaged along the line of sight) and plasma luminosity photographs suggest the presence of various types of nonuniformities.

I. Introduction

The MHD disk generator is a single-load device of circular symmetry without the numerous electrodes associated with linear generators. This allows larger electric fields to be sustained, yielding greater power densities when compared to linear generators. The radial flow disk is a pure Hall-effect device; however, with inlet swirl a substantial increase in electrical efficiency can be realized for moderate values of Hall coefficients. When driven with argon, and with swirl provided by inlet guide vanes, a strong reduction in the effective Hall parameter was observed. This reduction has been associated partially with the presence of wakes emanating from the trailing edge of the vanes and with ionizational nonuniformities resulting from large values of the ideal Hall parameter (>2).^{1,2}

The goal of the present study is to quantify the effects of the coupling of the nonequilibrium ionization process to the wakes. The first phase of the study is to establish the condition of the nonequilibrium plasma without wakes present and compare experimental results to the results from an analytical model. The conditions for the experiments described below were selected to obtain an ideal Hall parameter less than the critical value for ionizational stability. This would allow the model to be assessed without regard to particular reduction formulae. The analytical model is described and comparison of the theoretical to experimental results is discussed.

II. Analytical Model

The analysis of the nonequilibrium processes is performed with a quasi-one-dimensional, two-temperature fluid model which includes the effects of nonuniformities and finite rate kinetics and energetics. The nonuniformities, which develop during the ionization process, are important since the reduction of the electrical conductivity and Hall parameter affect the level of interaction and

length of the relaxation region. The length of the relaxation region is also governed by two rate processes: (1) the rate of ionization at low electron densities (10^{19} to 10^{20} m^{-3}) and (2) the rate of energy transfer at higher electron densities.

The effects of nonuniformities are incorporated through use of effective values for conductivity and Hall parameter in Ohm's law. For the case where no wakes are present and the plasma is initially uniform, the nonuniformities are assumed to arise from ionization instabilities only and develop instantaneously. For cases when the ideal Hall parameter (β_i) is not too large compared to the value at which the instability develops (β_{crit}), the analysis by Solbes³ is used to determine the effective conductivity (σ_e) and Hall parameter (β_e). This analysis has been shown to be valid when the nonuniformities are anisotropic (i.e., $\beta_i < 2 \beta_{crit}$).³ The extension of the model to analyze conditions of higher Hall parameter, is made by using reduction formulae for turbulent or isotropic nonuniformities. These are valid for a wide range of Hall parameter and distribution of nonuniformities. The applicability of both the anisotropic and the isotropic plasma model to the present experiments is discussed in Section III.

The rate processes are modeled by the kinetics and energy equations for the electrons. The kinetics are assumed to be dominated by three-body recombination reactions:

where species Y is the ionizable component in the gas and species X is the energy source or sink (third body) for the reaction. Since argon, nitrogen and cesium are considered by the model the following reactions are allowed:

For the case where the third body is an electron (e.g., equations 2 and 3), the recombination rate constant is given by Hinov and Hirschberg⁴ as $1.09 \times 10^{-20} / T_e^{4.5}$ m^6/sec where T_e is the electron temperature. The rate constant used for reaction (4) is $5 \times 10^{-36} / T$ m^6/sec where T is the temperature of the nitrogen. Reference 6 briefly discusses the origin of this rate constant and has found the reaction (4) to be important when nitrogen is the major component of the working fluid and the electron density is low ($\sim 10^{19}$ m^{-3}). The differential form of the rate equations for a singly ionized species is:

$$\frac{dN_e}{dt} = (K_s N_Y - N_e^2) K_r N_X \quad (5)$$

where N is the species number density, K_s is the Saha equilibrium constant, and K_r is the recombination rate for the particular three-body process. The electron energy equation can be expressed as:

$$\frac{d}{dr} (N_e U_e (3/2 k T_e + e_1) + p_e / Ne) = j^2 / \sigma - \text{COLL-RAD} \quad (6)$$

where the subscript refers to electrons and U_e , T_e , k , ϵ_1 , p , J , σ and r are the velocity, temperature, Boltzmann's constant, ionization potential, pressure, current density, effective conductivity and local radius, respectively. The collision term accounts for inelastic, as well as elastic losses. This allows the effects of molecular gases to be included not only as contaminants in inert gases but also as the working fluid. The radiation losses are assumed to be from the cesium resonance lines (8521 Å and 8943 Å) and are computed by the method of Lutz⁷. The optical depth of the plasma at these wavelengths is calculated and compensation is made for absorption effects. Radiation losses are important in the relaxation region where the neutral cesium density is high. The relative contribution from the continuum radiation was found to be negligible for typical generator operating conditions.

Both rate processes are important in the non-equilibrium generator where the electron density can span up to three orders of magnitude from the inlet to the exit. This point can be seen by looking at the characteristic times for electron temperature (τ_T) and electron density (τ_N) evolution. From a simple energy balance,

$$\tau_T = N_e \frac{k T_e}{J^2 \sigma} \quad (7)$$

and from the kinetic equation for small degrees of ionization

$$\tau_N = 1/(K_r N_e^2) \quad (8)$$

Evaluating these rates for two conditions typical of the experiments discussed in Section III and assuming a constant current density of 10^4 Amp/m² we have:

$$\begin{array}{ll} 1) \text{ at } & T_e = 2150 \text{ K} \\ & N_e = 1.1 \times 10^{20} \text{ m}^{-3} \\ & \tau_T = 6.2 \times 10^{-7} \text{ sec} \\ & \tau_N = 7.5 \times 10^{-6} \text{ sec} \\ 2) \text{ at } & T_e = 2500 \text{ K} \\ & N_e = 5.3 \times 10^{20} \text{ m}^{-3} \\ & \tau_T = 1.2 \times 10^{-5} \text{ sec} \\ & \tau_N = 6.4 \times 10^{-7} \text{ sec} \end{array}$$

The characteristic ionization time in Case 1 is governed by the kinetic rate whereas in Case 2, ionization is controlled by the rate of energy input. Since these conditions often co-exist within the generator, both processes are considered.

Calculations were made with the model, using the reduction formulae for ionizational nonuniformities, to show the effects of magnetic field and contaminants on the ionization level. The stagnation conditions were $T_0 = 2275$ K, $P_0 = 10$ atm. The seed fraction was .4% and the magnetic field was 1.35 T. Figure 1 shows the effects of magnetic field on the ionization. The solid curves represent the kinetic-dependent electron density and the dashed curves represent the electron density for Saha equilibrium at the electron temperature. As the magnetic field decreases the relaxation length increases greatly and the level of ionization decreases. The maximum degree of ionization for these conditions was approximately 15%. Figure 2 shows the effects of nitrogen addition on the electron density. A small amount of nitrogen (.5% or 5000 ppm) noticeably increases the relaxation length. A significant reduction in

performance would result in a small-scale generator (<1 m). This is in agreement with the experimental results of Zlatanovic et al.,⁸ who conducted tests in an Ar + Cs + N₂ plasma and found a significant reduction in performance at low stagnation temperature (~2000 K) with N₂ impurity levels of .3%.

III. Experiments and Analysis

The experiments discussed in this section were performed with the radial flow disk generator shown in Figure 3. The end wall of the generator is made of plexiglass (including the nozzle) so that flow visualization and voltage measurements can be made from the shock tube to the exit of the generator. Radial voltage profiles were obtained to determine the effective Hall parameter. Azimuthal voltage measurements and high-speed (~5000 frames/s) movies were made to assess the level of uniformity in the θ -direction. Continuum measurements at 4900 Å were made in the shock tube and up to seven different locations in the generator ranging from within one centimeter of the inlet to the exit (Figure 3). This technique for determining the electron density and the level of electron density fluctuations is a direct measurement although averaged along the line of sight. The electron temperature, required for reducing the continuum intensity measurement to electron density, is determined using the Saha equation where kinetic equilibrium exists and dN_e/dT_e is large. When the electron densities are low (i.e., during the initial relaxation) or when the seed is near full ionization, the electron temperature is computed by the analytical model. This method is satisfactory since the relation between continuum radiation and electron temperature is weak relative to the relation to electron density for the test conditions discussed below. The average seed fraction of the test gas is determined using Saha's equation with the outflow-corrected stagnation temperature behind the reflected shock and the electron density measured in the stagnation region.

Tests were conducted in order to check the validity of the model described in Section II. The conditions for the experiments were selected to produce a stable plasma (i.e., $\beta_i < \beta_{crit}$). This enabled an assessment of both the reference flow uniformity and the validity of the model without regard to particular reduction formulae calculations. The first series of tests were conducted at the following conditions: $T_0 = 2920$ K, $P_0 = 8.5$ atm, $B = 0.9$ T, seed fraction = .07%. The seed fraction is low and, therefore, the cesium becomes fully ionized near the generator inlet. This eliminates the effects of ionizational nonuniformities. The second series of tests were conducted at a high seed fraction and low stagnation temperature to obtain results when the seed is not fully ionized. Those conditions were: $T_0 = 2275$ K, $P_0 = 10$ atm, $B = 1.35$ T, seed fraction = .4%. The test conditions were also chosen to insure that MHD interaction-induced shocks were not present in the generator and static pressure measurements were made to monitor the level of interaction. The average Mach number in argon for low interaction was 1.8. All experiments were conducted at open circuit so the effective Hall parameter could be determined from the voltage profile.

The results of the high temperature tests with low seed fraction are summarized in Figures 4 thru

8. The low seed fraction insured that the ideal Hall parameter was less than the critical value throughout the generator (Figure 4), and resulted in fully ionized cesium at a radius of approximately 10. cm. This is evident from the small temporal variation of electron density shown in Figure 5. The azimuthal rms variation in voltage of approximately 4% seems to be moderate since no significant reduction in the Hall parameter was observed. Also, the flow appeared to be uniform as shown in Figure 6. Figure 6 is a reprint from a high-speed movie frame. The camera was viewing a portion of the generator test section and the shock tube. The experimental electron density and voltage profiles are compared with those predicted by the model in Figures 7 and 8, respectively. Agreement is good in both cases. The full ionization of the seed is apparent in the electron density profile as the level drops near the exit, since it now follows the gas pressure. This effect is underestimated by the model, possibly due to a small difference in interaction related to weak background nonuniformities.

The results for the low temperature tests are summarized in Figures 9 thru 17. The experimental electron density results in Figure 9 show that the inlet electron density is approximately 10^{20} m^{-3} . If no MHD interaction or radiative heat transfer takes place upstream of the throat and with the plasma in equilibrium in the stagnation region, the initial electron density would be approximately 10^{19} m^{-3} . The initial electron density for the theoretical calculations in Figure 9 was chosen to obtain the value measured near the generator inlet at 7.7 cm. The electron density and voltage profiles labeled curve 1 of Figures 9 and 10 are results of a full calculation by the model. The Hall parameter profiles are shown in Figure 11 which indicate the plasma is stable for $R > 7.3$ cm. Curves 2 of Figures 9 and 10 are results of a semi-empirical calculation using the experimentally determined effective Hall parameter and the assumption $\sigma_{\text{eff}}/\sigma = \beta_{\text{eff}}/\beta$. The Hall parameter profiles for this case are shown in Figure 12. Inspection of Figure 12 reveals that the electron density plateau observed experimentally (Figure 9) at $R = 10.5$ cm coincides with a strong reduction in the effective Hall parameter ($\beta/\beta_1 = .206$). Also, the plasma is predicted to be stable beyond $R = 9.3$ cm. Nonuniformities persist throughout the generator, however, and are indicated in Figures 13 and 14. Figure 13 is an electron density history measured at a radius of 13.5 cm, and Figure 14 is from a high-speed movie frame. Spokes are evident in the luminosity photograph. The model, which considers only ionizational nonuniformities in this case, is not able to predict the effective plasma properties. This implies that the formulation used to determine these properties is not applicable and the nonuniformities need to be better characterized.

The information related to the nonuniformities in the low stagnation temperature experiments is contained primarily in the electron density measurements shown in Figures 15 thru 17. Figure 15 compares the signal from two photomultiplier tubes located approximately 35° apart at a radius of 13.5 cm. The signals are very similar and there is little phase shift between them. A third signal obtained at a radius of 13.5 cm and between the two in Figure 15 gave similar results. Figure 16 is a comparison between signals obtained at different radial locations, 5.8 cm apart and along

the same azimuth. The similarity is not as strong as in the azimuthal direction and a phase shift of 45-50 μsec has been measured. The average frequency over several tests was 10 kHz, and the average rms of fluctuations is 40%. Figure 17 is a plot of the rms fluctuations. Note that the growth and decay of the fluctuations reflect the pattern of the effective Hall parameter in Figure 12. From these measurements it is difficult to define the structure of the nonuniformity. The uniformity of the electron density in the azimuthal direction (Figure 15) as well as the phase shift occurring in the radial direction (Figure 16) suggest a radial nonuniformity. If this were the case and the wavelength were greater than the distance between probes, the velocity of the wave would be ~ 1300 m/s and its wavelength ~ 13 cm. Additional experiments need to be made with a streak camera, however, to better define the wave structure. Theoretically, various types of waves can exist under the plasma conditions (i.e., elevated electron temperature and low Hall parameter (~ 1.0)) for the low temperature tests. Magnetoacoustic, electrothermal (ionization), and thermal waves were studied by Houghen and McCune⁷, who included the coupling between electrons and heavy particles. Based on their results, the following possibilities exist. Magnetoacoustic waves could propagate either antiparallel to the azimuthal current (spokes) or against the flow, depending upon the type of initial disturbance. These waves are not, however, detected by the pressure measurements and they are not likely to exist at the wavelengths (10 cm) required for their phase velocity (~ 650 m/s) and direction of propagation ($\pi/2 < \theta < \pi$; θ measured from the azimuthal current) to be compatible with the frequency of the disturbance (10 kHz). Ionization waves would be convected with the flow, and oriented at approximately $\theta = \pi/4$. As discussed above, however, growth occurs when the Hall parameter is greater than the critical value for the instability and this condition is only satisfied at the generator's entrance (Figure 12). However, the stability criterion could be altered by the presence of other waves or nonuniformities (Figure 14). Finally, a thermal wave could exist which would also be convected with the flow and oriented at $0 < \theta < \pi$. The growth rates are large, even at low Hall parameters, when the electron temperature is elevated. The wavelengths of the thermal waves are consistent with the flow velocity, orientation and observed frequency. Considering the large angle of orientation over which these waves exhibit growth, a radial disturbance (i.e., from the shock tube) could initiate a thermal wave. Table I is a summary of the conditions described above.

Despite the discrepancies encountered in the low stagnation temperature tests, two encouraging points emerge: (1) the model was able to reproduce quite well the experimental electron density profile when supplied with the measured effective Hall parameter and (2) high levels of nonequilibrium ionization ($\sim 2 \times 10^{20} \text{ m}^{-3}$) were obtained despite the background nonuniformities present in the generator. Additional work is required in order to establish a more uniform reference plasma before better agreement between calculated and measured effective Hall parameters can be obtained for these conditions.

IV. Summary and Conclusions

A description of the quasi-one-dimensional, two-temperature fluid model and experiments conducted to assess its validity were presented. The analytical model includes the effects of nonuniformities and finite rate kinetics and energetics. For nonuniformities arising from ignition instabilities, anisotropic formulations³ are used to obtain the effective values of conductivity and Hall parameter. The isotropic reduction formulae⁴ are used when the nonuniformities are turbulent. The finite rate kinetic processes consider three-body recombination, both electron and nitrogen as the third body, and the ionization of argon. The energy process is modeled with the effects of cesium resonance line radiation and inelastic, as well as elastic collisional losses. The inclusion of the inelastic collision losses allows the impact of contaminants in argon to be investigated, as well as a determination of the amount of non-equilibrium when molecular gases are the working fluid. A small addition of nitrogen (.5%) to the argon was shown to extend the relaxation region, which would reduce significantly the performance of a small-scale generator (<1 m). Experiments conducted at $T_0 = 2920$ K, $P_0 = 8.5$ atm, $B = .9$ T, and a seed fraction of .07% resulted in performance with $\beta_e = \beta_i$ throughout the generator. The electron density and voltage profiles predicted by the analytical model were in good agreement with the experimental results. A nonuniform plasma with rms electron density fluctuations of 40% at 10 kHz was observed at conditions of $T_0 = 2275$ K, $P_0 = 10$ atm, $B = 1.35$ T, and a seed fraction .4%. The full calculation performed with the model did not agree with the experimental results. The results of a semi-empirical calculation utilizing the experimentally determined effective Hall parameter did, however, agree with the measured electron density and voltage profiles. This indicates the reduction in the conductivity and Hall parameter is not due solely to the anisotropic ionizational nonuniformities assumed in the model. Additional experimental work is required to better characterize the nonuniformities.

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FIGURE 1. EFFECT OF MAGNETIC-FIELD (B) ON THE RELAXATION LENGTH. $T_0 = 2275$ K, $P_0 = 10$ ATM, $X = .4 \pm 2$ Cs
 — KINETIC-DEPENDENT ELECTRON DENSITY
 --- ELECTRON DENSITY IN SAHA EQUILIBRIUM AT THE ELECTRON TEMPERATURE

FIGURE 2. EFFECT OF H_2 CONTAMINANTS ON RELAXATION LENGTH.
 $T_0 = 2275$ K
 — KINETIC-DEPENDENT ELECTRON DENSITY
 --- ELECTRON DENSITY IN SAWA EQUILIBRIUM AT THE ELECTRON TEMPERATURE

500 $\mu\text{sec}/\text{cm}$ →

FIGURE 5. ELECTRON DENSITY HISTORY AT
 $R = 13.5$ CM. $T_0 = 2920$ K

FIGURE 6. PLASMA LUMINOSITY PHOTOGRAPH.
 $T_0 = 2920$ K

FIGURE 4. EFFECTIVE, IDEAL, AND CRITICAL HALL PARAMETER
 PROFILES. $T_0 = 2920$ K, $P_0 = 8.5$ ATM, $B = .9$ T, $X = .07$ %

FIGURE 7. CALCULATED ELECTRON DENSITY PROFILE AND EXPERIMENTAL
 RESULTS (THREE RUN AVERAGE). $T_0 = 2920$ K

FIGURE 8. CALCULATED VOLTAGE PROFILE AND EXPERIMENTAL RESULTS (THREE RUN AVERAGE). $T_0 = 2920$ K

FIGURE 9. CALCULATED ELECTRON DENSITY PROFILES AND EXPERIMENTAL RESULTS (FIVE RUN AVERAGE). $T_0 = 2275$ K, $P_0 = 10$ ATM, $B = 1.35$ T, $\chi = .4\%$.
(1) — FULL MODEL CALCULATION
(2) --- SEMI-EMPIRICAL CALCULATION

	MAGNETO-ACOUSTIC	IONIZATION	THERMAL
WAVELENGTH	< 1 CM	> 1 CM	> 1 CM
HALL PARAMETER	1 to 4	4	1
PHASE VELOCITY	a_0	$\ll a_0$	$\ll a_0$
ORIENTATION (ref. to azimuthal current)	$\frac{\pi}{2} < \theta < \pi$	$\frac{\pi}{4} < \theta < \frac{\pi}{2}$	$0 < \theta < \pi$

TABLE I. MHD WAVES FOR $(T_e - T_g) / T_g = 1$.

a_0 = acoustic velocity, T_e = electron temperature, T_g = gas temperature (Ref. 9)

FIGURE 10. CALCULATED VOLTAGE PROFILES AND EXPERIMENTAL RESULTS (FIVE RUN AVERAGE). $T_0 = 2275$ K
(1) — FULL MODEL CALCULATION
(2) --- SEMI-EMPIRICAL CALCULATION

FIGURE 11. EFFECTIVE, IDEAL, AND CRITICAL HALL PARAMETER PROFILES FROM THE FULL MODEL CALCULATION. $T_0 = 2275$ K

Figure 3. Assembly Drawing of the Radial Flow Generator for Controlled Studies of Nonequilibrium and Nonuniformities.

FIGURE 12. EFFECTIVE, IDEAL, AND CRITICAL HALL PARAMETER PROFILES FROM THE SEMI-EMPIRICAL CALCULATION. $T_0 = 2275$ K

FIGURE 13. ELECTRON DENSITY HISTORY AT $R = 13.5$ CM. $T_0 = 2275$ K

FIGURE 14. PLASMA LUMINOSITY PHOTOGRAPH. $T_0 = 2275$ K

FIGURE 15. ELECTRON DENSITY HISTORY FROM PROBES LOCATED 35° APART AT $R = 13.5$ CM. $T_0 = 2275$ K, $100 \mu\text{sec/cm}$ →

FIGURE 16. ELECTRON DENSITY HISTORY FROM PROBES LOCATED 5.8 CM APART ON THE SAME AZIMUTH. $T_0 = 2275$ K, $100 \mu\text{sec/cm}$

FIGURE 17. ELECTRON DENSITY FLUCTUATIONS (RMS No. s). $T_0 = 2275$ K

Appendix B

Small Perturbation Analysis of Wake Nonuniformities

Summarized below are the pertinent governing equations for a plasma in a disk generator with nonuniformities in the disk plane (r, θ) - and their small perturbation (linearized) approximation.

Continuity equation:

$$\frac{\partial}{\partial r} [\rho r W U_r] + \frac{\partial}{\partial \theta} [\rho W U_\theta] = 0, \quad W = W(r) = \text{channel height} \quad (1)$$

Momentum equations:

$$\rho \left[U_r \frac{\partial U_r}{\partial r} + \frac{U_\theta}{r} \frac{\partial U_r}{\partial \theta} - \frac{U_\theta^2}{r} \right] = - \frac{\partial P}{\partial r} + J_\theta B \quad (2)$$

$$\rho \left[U_r \frac{\partial U_\theta}{\partial r} + \frac{U_\theta}{r} \frac{\partial U_\theta}{\partial \theta} + \frac{U_r U_\theta}{r} \right] = - \frac{1}{r} \frac{\partial P}{\partial \theta} + J_r B \quad (3)$$

Energy equation:

$$\rho \left[U_r \frac{\partial h_o}{\partial r} + \frac{U_\theta}{r} \frac{\partial h_o}{\partial \theta} \right] = \mathbf{E} \cdot \mathbf{J}, \quad h_o = h + \frac{U_r^2 + U_\theta^2}{2} \quad (4)$$

Equation of state: $\rho = \rho(P, T)$

Electrodynamic Equations:

$$\frac{\partial}{\partial r} [r W J_r] + \frac{\partial}{\partial \theta} [W J_\theta] = 0$$

$$J_r + \beta J_\theta = \sigma (E_r + U_\theta B)$$

$$J_\theta - \beta J_r = \sigma (E_\theta - U_r B)$$

It is more convenient to use matrix notation for the fluid dynamics equation. If we introduce the unknown vector

$$\underline{U} = (U_r, U_\theta, P, T),$$

equations (1) - (4) can be written as

$$A \frac{\partial}{\partial r} \underline{U} + B \frac{\partial}{\partial \theta} \underline{U} + \underline{C},$$

where

$$A = \begin{bmatrix} \rho u_r & 0 & 1 & 0 \\ 0 & \rho u_r & 0 & 0 \\ \rho U_r^2 & \rho U_r U_\theta & C_T \rho U_r & C_P \rho U_r \\ \rho & 0 & \rho_T U_r & \rho_P U_r \end{bmatrix}$$

$$B = \begin{bmatrix} \rho U_\theta / r & 0 & 0 & 0 \\ 0 & \rho U_\theta / r & 1/r & 0 \\ \rho U_\theta U_r & \rho U_\theta^2 / r & \rho C_T U_\theta / r & \rho C_P U_\theta \\ 0 & \rho / r & \rho_T U_\theta / r & \rho_P U_\theta / r \end{bmatrix}$$

$$\underline{C} = \begin{bmatrix} J_\theta B + \rho \frac{U_\theta^2}{r} \\ -J_r B + \rho \frac{U_r U_\theta}{r} \\ \underline{E} \cdot \underline{J} \\ -\rho \frac{\partial}{\partial r} \ln(rW) \end{bmatrix}, \text{ and}$$

$$C_T = \left(\frac{\partial h}{\partial P} \right)_T, C_P = \left(\frac{\partial h}{\partial T} \right)_P, \rho_T = \left(\frac{\partial \rho}{\partial P} \right)_T, \rho_P = \left(\frac{\partial \rho}{\partial T} \right)_P.$$

For every variable quantity $f(r, \theta)$, we introduce the notation

$$f(r, \theta) = f^0(r) + f^1(r, \theta) \quad (5)$$

where $f^0(r)$ is the zero th order and $f^1(r, \theta)$ is the first order approximation. Applying the notation (5) in the fluid dynamic equations, one gets

zero th order approximation (Basic flow equation - no azimuthal nonuniformities):

$$A^0 \frac{\partial}{\partial r} \underline{U}^0 = \underline{C}^0,$$

first order approximation: (perturbed flow equation)

$$A^0 \frac{\partial}{\partial r} \underline{U}^1 + B^0 \frac{\partial}{\partial \theta} \underline{U}^1 + D_1^0 \underline{U}^1 = \underline{C}^1,$$

where

$$A^0 = \begin{bmatrix} \rho^0 U_r^0 & 0 & 1 & 0 \\ 0 & \rho^0 U_r^0 & 0 & 0 \\ \rho^0 (U_r^0)^2 & \rho^0 U_r^0 U_\theta^0 & C_T^0 \rho^0 U_r^0 & C_P^0 \rho^0 U_r^0 \\ \rho^0 & 0 & \rho_T^0 U_r^0 & \rho_P^0 U_r^0 \end{bmatrix},$$

$$B^0 = \frac{1}{r} \begin{bmatrix} \rho^0 U_\theta^0 & 0 & 0 & 0 \\ 0 & \rho^0 U_\theta^0 & 1 & 0 \\ \rho^0 U_\theta^0 U_r^0 & \rho^0 (U_\theta^0)^2 & \rho^0 C_T^0 U_\theta^0 & \rho^0 C_P^0 U_\theta^0 \\ 0 & \rho^0 & \rho_T^0 U_\theta^0 & \rho_P^0 U_\theta^0 \end{bmatrix},$$

$$\underline{C}^0 = \begin{bmatrix} J_\theta^0 B + \frac{\rho^0}{r} U_\theta^2 \\ -J_\theta^0 B + \rho^0 U_r^0 U_\theta^0 \\ \underline{E}^0 \cdot \underline{J}^0 \\ -\rho^0 \frac{\partial}{\partial r} \ln(rW) \end{bmatrix},$$

$$D_1^0 = \begin{bmatrix} \rho^0 \frac{\partial u_r^0}{\partial r} & \frac{1}{r} \rho^0 U_\theta^0 & 0 & 0 \\ \rho^0 \frac{\partial u_\theta^0}{\partial r} + \frac{1}{r} \rho^0 U_r^0 & \frac{1}{r} \rho^0 U_r^0 & 0 & 0 \\ 2 \rho^0 U_r^0 \frac{\partial U_r^0}{\partial r} + \rho^0 U_\theta^0 \frac{\partial U_\theta^0}{\partial r} + C_{T\rho}^0 \frac{\partial P^0}{\partial r} + C_{P\rho}^0 \frac{\partial T^0}{\partial r} & \rho^0 U_r^0 \frac{\partial u_\theta^0}{\partial r} & 0 & 0 \\ \rho^0 \frac{\partial P^0}{\partial r} + \rho_P^0 \frac{\partial T^0}{\partial r} & 0 & 0 & 0 \end{bmatrix}$$

$$C^1 = [C_1, C_2, C_3, C_4],$$

$$C_1 = J_\theta^1 + \frac{1}{r} \rho^1 (U_\theta^0)^2 - \rho^1 U_r^0 \frac{\partial U_r^0}{\partial r}$$

$$C_2 = -J_r^1 + \frac{1}{r} U_r^0 U_\theta^0 - \rho^1 U_r^0 \frac{\partial U_\theta^0}{\partial r}$$

$$C_3 = E^0 \cdot J^1 + E^1 \cdot J^0 - \left\{ \rho^1 (U_r^0)^2 \frac{\partial U_r^0}{\partial r} - \rho^1 U_r^0 U_\theta^0 + (C_{T\rho}^0 U_r^0 + C_{T\rho}^1 U_r^0) \frac{\partial \rho^0}{\partial r} \right. \\ \left. + (C_{P\rho}^0 U_r^0 + C_{P\rho}^1 U_r^0) \frac{\partial T^0}{\partial r} \right\}$$

$$C_4 = -\rho^0 \frac{\partial \ln(rW)}{\partial r}$$

Similarly, the electrodynamic equations gives:

Zeroth order approximation:

$$J_r^0 = \frac{I}{2\pi rW}, \quad I = \text{total current}$$

$$J_\theta^0 = -\sigma^0 B U_r^0 + \beta^0 J_r^0$$

$$E_r^0 = \frac{1}{\sigma^0} [J_r^0 + \beta^0 J_\theta^0 - B U_\theta^0]$$

1st order approximation:

$$\begin{aligned} \frac{\partial}{\partial r} [r W J_r^1] + \frac{\partial}{\partial \theta} [W J_\theta^1] &= 0, E^1 = -\nabla \phi^1 \\ J_r^1 + \beta^0 J_\theta^1 + \beta^1 J_\theta^0 &= \sigma^1 [E_r^1 + BU_\theta^1] + \sigma^1 [E_r^0 + BU_\theta^0] \\ J_\theta^1 - \beta^0 J_r^1 - \beta^1 J_r^0 &= \sigma^0 [E_\theta^1 - BU_r^1] - \sigma^1 BU_r^0. \end{aligned}$$

	MAGNETO- ACOUSTIC	IONIZATION	THERMAL
WAVELENGTH	< 1. CM	> 1. CM	> 1. CM
HALL PARAMETER	1 to 4	4	1
PHASE VELOCITY	a_o	$\ll a_o$	$\ll a_o$
ORIENTATION (ref. to azi- muthal current)	$\frac{\pi}{2} < \theta < \pi$	$\frac{\pi}{4} < \theta < \frac{\pi}{2}$	$0 < \theta < \pi$

TABLE II. MHD WAVES FOR $(T_e - T_g) / T_g = 1$.

a_o = acoustic velocity, T_e = electron temperature, T_g = gas temperature (Ref. 10)

N	$\text{rms}\sigma_1$	$\text{rms}\sigma_2$	α	$V_{\text{o.c.}}/V'_{\text{o.c.}}$
25	.13	.13	1	.79
	.13	.20	4	.61
50	.18	.18	1	.61
	.18	.29	4	.42

(a) $B = 1.0 \text{ T}$ ($1.27 \leq \beta \leq 2.39$)

N	$\text{rms}\sigma_1$	$\text{rms}\sigma_2$	α	$V_{\text{o.c.}}/V'_{\text{o.c.}}$
25	.13	.13	1	.52
	.13	.20	4	.34
50	.18	.18	1	.31
	.18	.29	4	.18

(b) $B = 2.0 \text{ T}$ ($2.54 \leq \beta \leq 4.67$)

Table III. Open-circuit voltage reduction dependence on number of blades ($N = 25, 50$) and wake width ($\alpha = 1, 4$) for $B = 1.0 \text{ T}$ and 2.0 T ; $80\% \text{ Ar}-20\% \text{ CO}_2$, $T_o = 2500 \text{ K}$, $p_o = 6.0 \text{ atm}$, $\chi = .16\% \text{ Cs}$.

Figure 1. Summary flow diagram illustrating loft performance relative to a typical program for the design of a combustion (steady-state) disk generator.

Fig. 2a. Effect of symmetric conductivity wakes ($w=1, N=12, \langle \sigma \rangle = 9$) on open circuit voltage. Values normalized by those for uniform plasma at $\langle \sigma \rangle$.

Fig. 2b. Effect of symmetric conductivity wakes on maximum electrical efficiency.

Table I

Comparison of Electrical Characteristics ($\beta=4$)
 $\sigma_1=10, \sigma_2=8$ for analytical case;
 $\sigma_0=9, \sigma_1=\sqrt{2}$ for numerical case

	$V_{Oc} (V)$	$I_{sc} (A)$	η_{max}	V at η_{max}
Uniform $\sigma = \langle \sigma \rangle$	687.5	158.1	.610	134.2
Rosa formulae	567.0	154.0	.574	118.3
Analytical (360 wakes)	567.9	156.1	.573	119.0
Numerical (360 wakes)	568.8	156.0	.579	118.9
Analytical (12 wakes)	610.4	156.9	.592	124.6
Numerical (12 wakes)	612.5	156.9	.588	122.7

Figure 3. Photographs of the nonuniformities generator walls.

Figure 4. Assembly drawing of the radial flow generator (before modifications) for controlled nonuniformities studies.

Figure 5 . New assembly drawing of the radial flow generator.

FIGURE 6. PHOTOGRAPHS OF NONUNIFORM REFERENCE FLOW FOR TESTS IN N_2 . ($T_0 = 2900$ K)

FIGURE 7. EFFECTIVE, IDEAL, AND CRITICAL HALL PARAMETER PROFILES. $T_0 = 2920$ K, $P_0 = 8.5$ ATM, $B = .9$ T, $\chi = .07$ %

FIGURE 8. ELECTRON DENSITY HISTORY AT
 $R = 13.5$ CM. $T_o = 2920$ K
($500\mu\text{sec/cm}$)

FIGURE 9. PLASMA LUMINOSITY PHOTOGRAPH.
 $T_o = 2920$ K

FIGURE 10. CALCULATED ELECTRON DENSITY PROFILE AND EXPERIMENTAL RESULTS (THREE RUN AVERAGE). $T_0 = 2920$ K

FIGURE 11. CALCULATED VOLTAGE PROFILE AND EXPERIMENTAL RESULTS
(THREE RUN AVERAGE). $T_0 = 2920$ K

FIGURE 12. CALCULATED ELECTRON DENSITY PROFILES AND EXPERIMENTAL RESULTS (FIVE RUN AVERAGE). $T_0 = 2275$ K, $P_0 = 10$ ATM, $B = 1.35$ T, $\chi = .4$ %.

- (1) — FULL MODEL CALCULATION
- (2) --- SEMI-EMPIRICAL CALCULATION

FIGURE 13. CALCULATED VOLTAGE PROFILES AND EXPERIMENTAL RESULTS (FIVE RUN AVERAGE). $T_0 = 2275$ K

- (1) - FULL MODEL CALCULATION
- (2) - SEMI-EMPIRICAL CALCULATION

FIGURE 14. EFFECTIVE, IDEAL, AND CRITICAL HALL PARAMETER PROFILES FROM THE FULL MODEL CALCULATION. $T_0 = 2275$ K

FIGURE 15. EFFECTIVE, IDEAL, AND CRITICAL HALL PARAMETER PROFILES FROM THE SEMI-EMPIRICAL CALCULATION.
 $T_0 = 2275$ K

FIGURE 16. ELECTRON DENSITY HISTORY AT
 $R = 13.5 \text{ CM. } T_o = 2275 \text{ K}$
(500 $\mu\text{sec/cm}$)

FIGURE 17. PLASMA LUMINOSITY PHOTOGRAPH.
 $T_o = 2275 \text{ K}$

FIGURE 18. ELECTRON DENSITY HISTORY FROM PROBES
LOCATED 35° APART AT $R = 13.5$ CM.
 $T_o = 2275$ K

FIGURE 19. ELECTRON DENSITY HISTORY FROM PROBES
LOCATED 5.8 CM APART ON THE SAME
AZIMUTH. $T_o = 2275$ K

FIGURE 20. ELECTRON DENSITY FLUCTUATIONS (RMS Ne).
 $T_0 = 2275$ K

Figure 21. Calculated voltage profiles and experimental results for Ar/CO tests ($T = 2800$ K, $p = 5.7$ atm, $B = 2.0$ T, $\chi = 0.16\%$ ^{40}Cs , $\delta_{\text{CO}} = 230$)

- (1) - full model calculation
- (2) - semi-empirical calculation

Figure 22. Effective, ideal, and critical Hall parameter profiles from the full model calculation ($T_0 = 2800$ K).

Figure 23. Effective, ideal, and critical Hall parameter profiles from the semi-empirical calculation ($T_0 = 2800$ K).

Figure 24. Calculated and measured static pressure profiles for the Ar/CO tests ($T_o = 2800$).

Figure 25. Calculated electron density profiles for the Ar/CO tests ($T_0 = 2800$ K); dashed curves are Saha₀ values.

Figure 26. Characteristic ionization time/flow time ratio versus radius for Ar/CO tests ($T_o = 2800$ K, $\tau_{fl} = 50\mu\text{sec}$)

Figure 27. PLASMA LUMINOSITY PHOTOGRAPH FOR Ar/CO TEST
($T_0 = 2800$ K, $B = 2.0$ T).

Figure 28. Calculated (with full model) and measured effective Hall parameter comparison with ideal Hall parameter for the Ar/CO tests ($T_o = 2800$ K).

$R = 9.2 \text{ cm}$

$R = 11.4 \text{ cm}$

$R = 12.9 \text{ cm}$

Figure 29. Voltage histories at different radial locations for Ar/CO tests ($T_0 = 2800 \text{ K}$, $B = 2.44 \text{ T}$; 0.2 msec/div).

Figure 30. Effect of stagnation temperature and magnetic field on maximum (at $R = 15$ cm) ionization/flow time ratio for the Ar/CO tests.

Figure 31. Calculated voltage profiles and experimental results for Ar/CO tests ($T = 3700$ K, $p = 6.5$ atm, $B = 3.5$ T, $\chi = 0.043\%$ Cs, $\delta_{CO} = 230$)

- (1) - full model calculation
- (2) - semi-empirical calculation

Figure 32. Effective, ideal, and critical Hall parameter profiles from the full model calculation ($T_o = 3700$ K).

Figure 33. Hall parameter profiles from the semi-empirical calculation ($T_o = 3700$ k, $\delta_{CO} = 230$).

Figure 34. Calculated and measured static pressure profiles for the Ar/CO tests ($T_o = 3700$, $B = 3.5$ T).

Figure 35. Calculated electron density profiles for the Ar/CO tests ($T_e = 3700$ K); dashed curves are Saha values.

Figure 36. Characteristic ionization time/flow time ratio (for a 40% increase in Ne) versus radius for Ar/CO tests ($T_o = 3700$, $B = 3.5$ T, $\tau_{fl} = 50\mu\text{sec}$).

Figure 37. Calculated (with full model) and measured effective Hall parameter comparison with ideal Hall parameter for the Ar/CO tests ($T_0 = 3700$ K).

$R = 9.2 \text{ cm}$
(decreased
sensitivity)

$R = 11.4 \text{ cm}$

$R = 12.9 \text{ cm}$

Figure 38. Voltage histories at different radial locations for Ar/CO tests ($T_o = 3700 \text{ K}$, $B = 3.5 \text{ T}$; 0.2 msec/div).

$R = 9.2$ cm

$R = 9.8$ cm

$R = 12.9$ cm

Figure 39. Voltage histories at different radial locations for Ar/CO tests ($T_0 = 3700$ K, $B = 1.6$ T; 0.2 msec/div).

Figure 40. Calculated voltage profiles and experimental results for Ar/CO tests ($T = 3700$ K, $p = 6.5$ atm, $B = 3.5$ T, $\chi = 0.043\%$ C_s , $\delta_{CO} = 500$); illustrates effect of loss factor when compared to Figure 31.

- (1) - full model calculation
- (2) - semi-empirical calculation

Figure 41. Hall parameter profiles from the semi-empirical calculation ($T = 3700$ K, $\delta = 500$); illustrates effect of loss factor when compared to Figure 33.

Figure 42. Comparison of calculated and measured electron density at short-circuit ($I_{s.c.} = 65$ amps); open-circuit calculations are shown for reference, and illustrate the extent of the MHD interaction.

Figure 43. Calculated and measured voltage profiles for Ar/CO₂ tests
 ($T_o = 2900$ K, $p_o = 6.5$ atm,
 $B = 2.6$ T, $\chi = 0.16\%$ Cs, $\delta_{CO_2} = 2500$)

Figure 44. Hall parameter profiles from the full model calculation for the Ar/CO₂ tests ($T_o = 2900$ K, $B = 2.6$ T).

Figure 45. Calculated electron density profile for the Ar/CO₂ tests ($T_e = 2900$ K, $B = 2.6$ T); electrons are in Saha equilibrium (see dashed curve).

Figure 46. Calculated characteristic 40% ionization time/flow time ratio (τ/τ_{fl}) for the Ar/CO₂ tests ($T_o = 2900$ K, $B = 2.6$ T, $\tau_{fl} = 250\mu\text{sec}$).

Figure 47. Calculated (with full model) and measured effective Hall parameter comparison with ideal Hall parameter for the Ar/CO₂ tests ($T_o = 2900$ K).

(a) Present channel

(b) New channel

Figure 48. Scale drawing comparison of present channel and new channel configurations.

Figure 49. Comparison of calculated Hall parameter profiles for the present channel (dashed curves- see Figure 44) and the new channel (solid curves) shown in Figure 48 (Ar/CO₂ - $T_o = 2900$ K, $B = 2.6T$).

Figure 50. Comparison of calculated electron density profiles for the two channels shown in Figure 48 ($Ar/CO_2 - T_g = 2900$ K, $B = 2.6$ T); electrons are in Saha equilibrium.

Figure 51. Comparison of the 40% ionization time/flow time (τ/τ_{fl}) for the two channels shown in Figure 48 (Ar/CO₂ - $T_o = 2900$ K, $B = 2.6$ T, $\tau_{fl} = 50\mu\text{sec}$).

Figure 52: Electron density boundary layer profiles at the trailing edge of short laminar struts (Ar/CO_2 -
 $T = 2500 \text{ K}$, $\delta = 0.17 \text{ mm}$, $N_{e\infty} = 7.3 \times 10^{19} \text{ m}^{-3}$,
 $M_{\text{T.E.}}^0 = 1.0$, $\ell = 1.7 \text{ cm chord}$).

- (a) - Nonequilibrium Ne
- (b) - Saha Equilibrium Ne*

Figure 53. Electron density boundary layer profiles at the trailing edge of long (laminar with transition to turbulent) struts (Ar/CO_2 - $T = 2500$ K, $\delta = 0.64$ mm, $N_{e\infty} = 7.4 \times 10^{19} \text{ m}^{-3}$, $M_{\text{T.E.}} = 1.0$, $l = 7.7$ cm chord).

- (a) - Nonequilibrium Ne
- (b) - Saha Equilibrium Ne*

Figure 54. Conductivity profile for short strut case (see Figure 52, curve (a)).

Figure 55. Nonequilibrium boundary layer conductivity profile for long strut case (corresponds to curve (a) in Figure 53).

FIGURE 56. Maximum allowable swirl for numerical stability of the Dynamic Problem graphed against the total Mach number for several values of grid ratio, $r\Delta\theta/\Delta r$.

Figure 57. Schematic of a disk generator with conducting ring modules on the side wall.

Figure 58. Local View of a Wall Segment, in the Meridional Plane of a disk generator with conducting modules on the side wall

TASK 3, ELECTRODE MODULE DEVELOPMENT AND TESTING

PART A.

During this research period we wish to summarize the thermochemical and electronic property data for the SrZrO_3 - LaFeO_3 - SrFeO_3 system and the SrZrO_3 - $\text{LaFe}_{0.5}\text{Cr}_{0.5}\text{O}_3$ - $\text{SrFe}_{0.5}\text{Cr}_{0.5}\text{O}_3$ system.

Phase Equilibria of the SrZrO_3 - LaFeO_3 - SrFeO_3 System in Air

Figures III-1 to III-3 show the binary phase diagrams for the ternary system in air. Solid-solid solubility is indicated for two binaries above 1100°C . Figure III-4 shows the liquidus contours for the ternary (in air). The general trend is for more refractory compositions as the SrZrO_3 content increases. The last melting compositions are SrFeO_3 -rich. The 1300°C isotherm for the ternary is shown in Figure III-5, with the tie lines for the two-phase region shown as dashed lines.

Substitution of chromium for iron in the system increases the refractoriness but decreases the extent of the solid solution phases (Figures III-6 and III-7).

Electrical Properties in the SrZrO_3 - LaFeO_3 - SrFeO_3 System

The electrical conductivity and Seebeck voltages as a function of composition, temperature and oxygen pressure were measured. Figure III-8 shows the data for LaFeO_3 between 1000 and 1400°C , and in the range of oxygen pressures of 10^{-17} to 1 atmosphere. The Seebeck voltage is shown for the same conditions in Figure III-9. The Seebeck voltage is related to the carrier concentration while the conductivity is proportional to the concentration times the carrier mobility. A composite plot of Seebeck voltage and conductivity as a function of temperature is shown in Figure III-10. In the n-type region, the carrier concentration saturates and becomes tangential to the line $-\frac{k}{e} \ln 10$.

As 10% strontium is added to LaFeO_3 the p-type conductivity at high oxygen pressure saturates for all temperatures (Figure III-11). Further replacement of La by Sr continues to increase the conductivity (Figure III-12) and causes the predominant carrier type (holes, p-type) to be saturated at high oxygen pressures (Figure III-13 and III-14). Substitution of 90% Sr for La does not substantially improve the electrical properties (Figure III-15 and III-16), but does decrease the refractoriness (Figure III-1).

As SrZrO_3 is added to the binary, the refractoriness increases (Figure III-4) with a decrease in the conductivity. Figure III-17 shows the temperature dependence of the conductivity in air for compositions between the binary composition, 75% LaFeO_3 -25% SrFeO_3 and SrZrO_3 . With as much as 75% SrZrO_3 the temperature dependence of the conductivity becomes very small, with the value being about $0.1/\Omega\cdot\text{cm}$. Decreasing the SrZrO_3 to 50% increases the conductivity an order of magnitude and decreases the melting point to about 2200°C . Note in Figure III-5 from the tie-lines in the two-phase region, that the less refractory phase is more LaFeO_3 -rich for the 50% SrZrO_3 composition than the 75% SrZrO_3 composition. This means that the SrZrO_3 depleted phase also has a higher melting point. The oxygen pressure dependence of the conductivity of SrZrO_3 is given in Figure III-18.

Comparisons of the conductivity between the ternary and the quaternary containing chromium is shown in Figures III-19 and III-20.

Figure III-1. Binary phase equilibrium diagram in air of the SrFeO_3 - LaFeO_3 system.

Figure III-2. Binary phase equilibrium diagram in air of the LaFeO_3 - SrZrO_3 system.

Figure III-3. Binary phase equilibrium diagram in air of the SrFeO_3 - SrZrO_3 system.

Figure III-4. Liquidus contours in the SrFeO_3 - LaFeO_3 - SrZrO_3 system in air.

Figure III-5. Isothermal (1300°C) equilibrium in the SrFeO_3 - LaNiO_3 - SrZrO_3 system indicating a two-phase region extending from the SrFeO_3 - SrZrO_3 binary into the ternary.

Figure III-6. Binary phase equilibrium diagram in air for the $\text{La}_{0.75}\text{Sr}_{0.25}\text{Fe}_{0.5}\text{Cr}_{0.5}\text{O}_3$ - SrZrO_3 system. The dashed lines are for the $\text{La}_{0.75}\text{Sr}_{0.25}\text{FeO}_3$ - SrZrO_3 system.

Figure III-7. Two-phase region for the $\text{SrFe}_{0.5}\text{Cr}_{0.5}\text{O}_3 - \text{LaFe}_{0.5}\text{Cr}_{0.5}\text{O}_3 - \text{SrZrO}_3$ system in air at 1700°C.

Figure III-8. Electrical conductivity of LaFeO_3 .

Figure III-9. Seebeck voltage of LaFeO_3 .

Figure III-10. Composite curves from Figures III-8 and III-9.

Figure III-11. Seebeck voltage of $\text{La}_{0.9}\text{Sr}_{0.1}\text{FeO}_3$.

Figure III-12. Electrical conductivity of $\text{La}_{0.75}\text{Sr}_{0.25}\text{FeO}_3$.

Figure III-13. Seebeck voltage of $\text{La}_{0.75}\text{Sr}_{0.25}\text{FeO}_3$.

Figure III-14. Composite Seebeck conductivity plot for La_{0.75}Sr_{0.25}FeO₃.

Figure III-15. Electrical conductivity of La_{0.1}Sr_{0.9}FeO₃.

Figure III-16. Seebeck voltage of $La_{0.1}Sr_{0.9}FeO_3$.

$(\text{La}_{0.75}\text{Sr}_{0.25}\text{FeO}_3)_{1-x}(\text{SrZrO}_3)_x$ in air

Figure III-17. Temperature dependence of the $(\text{La}_{0.75}\text{Sr}_{0.25}\text{FeO}_3)_{1-x}(\text{SrZrO}_3)_x$ system in air.

Figure III-18. Electrical conductivity of $SrZrO_3$ of 1200-1600°C as a function of P_{O_2} .

Figure III-19. Electrical conductivity in the $\text{La}_{0.75}\text{Sr}_{0.25}\text{Fe}_{0.5}\text{Cr}_{0.5}\text{O}_3$ - SrZrO_3 system in air.

Figure III-20. Electrical conductivity in the $75\text{SrZrO}_3 - (\text{SrFe}_{0.5}\text{Cr}_{0.5}\text{O}_3)_{1-x} - (\text{LaFe}_{0.5}\text{Cr}_{0.5}\text{O}_3)_x$ system.

Insulator Materials

Lack of information about dielectric breakdown of insulator materials suitable for use in MHD electrode modules has directed initial work under this sub-task toward obtaining "base line" data for a few materials. It is also necessary to consider the effect of microstructure on the performance of any particular material. Ideally, for each material we would test three microstructures: single crystal, representative of "intrinsic" material properties; dense polycrystalline, representative of "best" likely commercial material; and porous polycrystalline, representative of an "engineering" material. It appears that not all materials will be available in each of the three forms mentioned.

Materials tested first include MgAl_2O_4 , MgO , Al_2O_3 , and SrZrO_3 . Wherever possible, commercially available materials were used. When a commercial supplier was not available, the materials were fabricated here at the MHD Electrode Fabrication Facility.

Specimen Preparation

Typical specimens range in size from a minimum of about 1.0 centimeter diameter and 0.25 millimeter thick, to a maximum size of about 2.0 centimeters square and 1.0 millimeter thick. Specimens are prepared by a variety of techniques, depending on the form of the "as received" material. Single crystals are cut and ground from boules using diamond tools, although some (such as MgO) are received as small, thin plates and need only minimal grinding to thickness. Sintered and hot pressed materials are usually also cut and ground, although we have made sintered SrZrO_3 specimens directly to size. Plasma sprayed materials are generally made directly to specimen size.

Testing

Testing for dielectric breakdown strength was done using thin, flat plates of material as specimens and small spherical electrodes as contacts. Room temperature measurements were made in an insulating oil medium. High temperature measurements were made in an air atmosphere in an electric resistance heated furnace.

A multi-annular, swirl burner for use with natural gas and air is being set up to provide a clean source of heat for testing insulators with an imposed thermal gradient. These tests will probably be done with electrode modules, or a configuration which resembles them closely.

Results

Results of preliminary tests of dielectric breakdown strength versus thickness, at room temperature, are shown in Figures 3.1 through 3.4. These tests were conducted to obtain "typical" values of dielectric strength for the materials data bases. From these data we were then able to determine suitable specimen thicknesses for testing at elevated temperature. In general, the dielectric strength of the materials tested behaved typically; that is, the strength decreased with increasing thickness. Also, the dielectric strength for porous material decreased relative to dense material.

Results of preliminary tests of breakdown voltage versus temperature are shown in Figures 3.5 through 3.8. These data show the decrease of breakdown voltage with increasing temperature, and indicate the change of breakdown mode from avalanche to thermal failure.

FIGURE 3.1

FIGURE 3.2

FIGURE 3.3

FIGURE 3.4

FIGURE 3.5

FIGURE 3.6

FIGURE 3.7

FIGURE 3.8

Electrode Module Testing

The duration of electrode tests performed in the MHD Simulation Facility at M.I.T. during this report period was increased from the previous length of two to six hours to as long as 17.5 hours. These were clean fired tests with seed but no slag, and were intended to be preparatory tests for super hot-wall tests with slag.

Two tests were performed during this report period. One on a single-layer, plasma-sprayed electrode of composition SZSFLF-3 ($75\% \text{SrZrO}_3 - 25\% \text{La}_{0.75} \text{Sr}_{0.25} \text{FeO}_3$) and the other on a multilayered plasma-sprayed electrode. The latter was a graded electrode with three layers: the layer adjacent to the stainless steel base was LASF5 ($\text{La}_{0.9} \text{Sr}_{0.1} \text{FeO}_3$); the next layer was a 2:1 mixture of SZSFLF-3 and LASF5; and the top layer was SZSFLF-3. Each layer was about 0.9 mm thick and was sprayed on a grooved stainless steel base. The single layer electrode was designed to be tested at $1/2 \text{ A/cm}^2$, while the multilayered electrode was designed for 1 A/cm^2 .

Electrodes were initially proof tested in the MHD Simulation Facility by heating them rapidly to 1000°C surface temperature and then cooling after a short duration at temperature. One of the stack of twelve multilayered electrodes failed during the proof test, so that in the actual test five anodes and five cathodes were used. Two sets of multilayered electrodes on pinned bases were proof tested also, and most of these electrodes failed by detachment of the ceramic from the pins.

Test 274: (single-layered electrodes) The duration of this test was 17 hours, 35 minutes, the surface temperature was $1800 \pm 100^\circ\text{C}$, and a current density of $1/2 \text{ A/cm}^2$ was maintained for 15 hours and 15 minutes. The test was terminated when one of the water cooling lines overheated from radiant heat and burst. This problem was subsequently corrected by

using a special reflecting insulation. At the time of termination, electrodes were performing well electrically and had not experienced any severe degradation as observed through the viewing port. Surfaces were no longer flat as judged by a variation in temperature from point to point on the electrode and one or two small pieces had detached. All these changes occurred in the first five hours of the test and remained unchanged for the remainder of the test. Microstructural examination will be discussed in the next report.

Tests 276, 277, 278: (multi-layered electrodes) The duration of this test was 13 hours and 15 minutes, the surface temperature again being approximately 1800°C. Current of 1 A/cm² was applied for 11 hours and 53 minutes. The test was terminated when the exhaust fan became clogged with potassium hydroxide seed. Without a vent for the plasma the chamber rapidly overheated and produced a shut-down. During the test there were two accidental sudden shut-downs caused by personnel allowing the fuel supply to run out. These shut downs and start-ups did only slight apparent damage to the electrodes. At termination of the test, the electrodes were functioning well at 1 A/cm². More degradation, as observed through the viewing port, occurred with the multi-layered electrodes than the single-layered electrodes. It appeared that the outer layer of most electrodes was lost during the first hour of testing. Nevertheless, electrodes continued to function well.

New Electrode Materials Development

Electrode materials with a higher electrical conductivity than the SZSFLF-3 would allow a single-layer electrode construction. This would be desirable from the point of view of reducing interlayer thermal mechanical stress, which our experience seems to indicate is responsible for some failures. Thus, work was undertaken to develop an equally refractory material, but with a conductivity a factor of ten higher, about $1\Omega^{-1}\text{cm}^{-1}$ over all temperatures. At this level of conductivity, the electrochemical effect, using our test conditions, would be negligible.

The possibility of improving electrical conductivity by substituting stable divalent ions for Sr was investigated. Results for the system $75\text{SrZrO}_3\text{-}25\text{La}_{x1-x}\text{FeO}_{3-\delta}$ are shown in Fig. 3.9. The substitution of Ca for Sr shows a slightly higher conductivity, but Ba decreases the conductivity. The conductivity of the $y = \text{Sr}$ compound measured in these specimens, however, is a factor of two lower than the previously reported results for the same composition, so it is difficult to evaluate the degree of improvement, but it is not significant in any case.

The P_{O_2} dependence varies from that of the SZSFLF-3, and is more favorable toward electrochemistry at the cathode. Figures 3.10 and 3.11 indicate an increased conductivity with lower P_{O_2} , except at the lower temperature, where a phase change apparently strongly affects the electrical conductivity.

The melting temperature of these three compounds were measured with a laser heat source in air. They are:

	M.P.
$75\% \text{SrZrO}_3\text{-}25\% \text{La}_{0.75}\text{Sr}_{0.25}\text{FeO}_3$	2450°C
$75\% \text{SrZrO}_3\text{-}25\% \text{La}_{0.75}\text{Ca}_{0.25}\text{FeO}_3$	2050°C
$75\% \text{SrZrO}_3\text{-}25\% \text{La}_{0.75}\text{Ba}_{0.25}\text{FeO}_3$	2150°C

In summary these materials show little advantage over the SZSFLF-3 already being tested as electrodes.

An examination of materials properties measured in the LaFeO_3 - SrFeO_3 - SrZrO_3 system and described in this section of the report, led us to consider the material designated SZSFLF-6 as a candidate for a single-layer electrode material. The composition is 50% SrZrO_3 -50% $\text{La}_{0.75}\text{Sr}_{0.25}\text{FeO}_3$ and the melting temperature is 2300°C . It has an electrical conductivity near $1\Omega^{-1}\text{cm}^{-1}$ independent of temperature down to 800°C . It is, however, two phase below 1500°C , whereas the two phase region extends only up to 1300°C for the SZSFLF-3. Although this is not quite as satisfactory in terms of mechanical stability of the electrode during heat-up, we feel the single layer will have superior mechanical stability to the multilayered electrode. Both are single phase at $> 1700^\circ\text{C}$, which is the surface temperature. Electrodes of this composition are thus presently being fabricated.

Figure 3.9. Four point probe D. C. electrical conductivity versus composition.

Figure 3.11. Four point probe D. C. electrical conductivity as a function of oxygen partial pressure.

Figure 3.10. Four point probe D. C. electrical conductivity as a function of oxygen partial pressure.

PART B.

THE MHD TEST FACILITY COMPLEX

The MHD Small Scale Simulation Facility, originally designed and used primarily for electrode development testing, has also been extensively used for other MHD-related tests.

Because the original installation is expanding to include the long projected addition of Channel 2, both of which will support equally well the Task 1 (Phenomena in MHD Generators) and Task 3 (Electrode Module Development) experimental phases, all facility related efforts, including also general instrumentation, data acquisition and processing, and others, are consolidated and reported under this title.

FACILITY DESCRIPTION

General Description of the Facility

This MHD Small Scale Simulation Facility is described in detail in Reference F-1. It is continuously being up-graded for a more dependable and wider range of operating conditions and progressively more advanced test techniques and diagnostic procedures. Considerable gain was realized in the reporting period in the realm of dependable operation.

Facility Objectives

The purpose of this facility is to provide a well controlled simulation of the plasma and electric field conditions in a coal-fired or clean base-load MHD generator. The principal design objectives for this facility were that it should allow for economical and rapid MHD materials

evaluation and for electrode/insulator module development as well as for good access for MHD, fluid mechanics and combustor studies, and for easy instrumentation access in general. This versatility has been extensively used in the past. In the present reporting period, it has made possible the installation of an enclosed, extensively instrumented rectangular channel designed to study boundary layer and heat transfer correlation in a high temperature gas stream flowing over a cool surface. More recently, the rectangular nozzle was fitted with a brick lined rectangular channel equipped with electrodes for induced current application and fitted with extensive electric instrumentation on the walls for the study of diffused and arcing current transfer phenomena.

The facility is currently being fitted with equipment for the study of boundary layer with upstream blowing as part of a Navy contract on turbine blade cooling.

FACILITY DEVELOPMENT

The MHD Simulation Facility continues to be improved as the needs of the program require. A series of measures implemented in the reporting period has made it possible to operate the facility for extended test times in response to the needs of the electrode development efforts.

Infrastructure

Continued MIT support in upgrading the supporting equipment permitted the implementation of a re-designed river water piping system entirely indoors. This was the necessary solution to the frequent problems of freezing pipes in the winter.

Another major improvement, made possible with MIT support, was the replacement of the city water pump with a new pump of higher capacity and higher pressure. The burner and electrodes cooling water flow has been raised from 50 psig to 75 psig, and the higher capacity will permit the use of more complex cooling and larger models than were possible before.

Facility Components

As with the infrastructure, improvements to the facility equipment proper are a continuous effort. The pressure transducers in the air and oxygen venturi flow meters were replaced with new, high quality differential pressure transducers for improved accuracy and dependability.

As a precaution in the operation of the scrubber system, particularly in view of the electrode development long endurance test phase just started, thermocouples were installed to continuously monitor the stream temperature at critical stations just upstream of the scrubber and also just upstream of the blower inlet.

The intense radiant heat on the electrode water cooling hoses required the use of thermal protection for the prolonged exposure during the long endurance tests. This was easily accomplished with the use of three layers of Minnesota Mining and Manufacturing Co. AB-312 braided fire sleeving.

An adjustable collar was installed at the top of the test section, where the capture duct slips inside the permanent exhaust duct, to negate recirculation of the hot combustion gases inside the test chamber. This yielded remarkable improvement in maintaining the test equipment at lower ambient temperatures.

For protection from accidental overheating conditions, as recently experienced when the exhaust blower failed during a test, all the control wiring into the test section was replaced with the highest temperature insulation wiring commercially available, and the electrical conduit was distanced from the hot tank wall with thermal spacers. The instrumentation wiring from the test section is being re-routed through thermally shielded passages inside and through the test section wall.

An uncooled section of the exhaust duct inside the cell, which was intended to be the joining section for a projected electrofluidized bed, releases excessive heat inside the cell. For relatively short tests this condition is inconsequential. However, during the recent endurance tests the amount of heat accumulated in the cell resulted in an untimely activation of the sprinkler system. A design for a water jacketed replacement section with internal water spray to cool the exhaust before it reaches the impactor scrubber is being readied for early installation.

The original liquid propane gas system in replaceable cylinders used to fuel the pilot burner was replaced with a direct supply of methane at high pressure.

A new attempt to draw the diluting air from outside the cell was frustrated by renewed noise complaints from the neighbouring buildings. To remedy this situation MIT is actively engaged in the procurement and installation of an air intake silencer.

A small amount of residue from the seeded plasma which the impactor scrubber does not remove accumulates in the narrow passages of the blower, progressively reducing its efficiency during a test. This is normally of no consequence for tests of a few hours. However, in a recent long endurance test, the blower finally lost power, rapidly aggravating itself to the extent that the emergency interlock system interrupted the test. To prevent reoccurrence of this condition a fog spray is being installed upstream of the blower in an attempt to keep the impeller continuously washed, thus, hopefully, correcting this situation.

FACILITY SAFETY

A recent safety reassessment by the MIT Safety Officer resulted in the issuance of a list of safety implementing recommendations. In response thereto, a full review of the Facility's operations has been initiated to try to find the most enduring and cost-effective way having the least impact on the progress of the experimental program for complying with the Safety Officer's dictum.

The expenses associated with implementing some of the measures will have to be borne by MIT, while others are within the domain of the project.

BURNER DEVELOPMENT

The burner has reached a state of development that renders very satisfactory test conditions and dependable operation. Nonetheless, the requirements for still better performance continue to grow, leading to a few burner improvements in the recent past.

The present short nozzle with abrupt area changes results in somewhat uneven seed distribution and thick slag run-off from the burner cylindrical wall. A preliminary design has been initiated for a new nozzle with a gradual transitional section from circular to rectangular. To be able to do this without increasing the burner's overall length, the top module will be eliminated and the nozzle initial transition will start from the original location of the sacrificed module.

The flat base plate has been replaced with a contoured surface base plate which should considerably reduce the rate of erosion on the corner. This new design is simpler to manufacture, costs less, and is easier to replace.

A new and similar base plate, made of stainless steel, is being designed. The purpose of this stainless steel base plate is two-fold: a) to explore the feasibility for a longer life cycle module using a higher seed corrosion-resistant metal; and b) to gain experience leading to the design of an all stainless steel burner that would operate at surface temperatures higher than the seed dew point.

In a continuous effort to find a suitable coating over copper that would resist seed erosion at the operating surface temperatures, two types of coatings have recently been investigated. One is the application of a sintered carbide applied by the detonation gun process (patented by Union

Carbide) and the other is sputter ion platinum plating. Both processes appear very promising, but no action has been taken.

The two single hole seed injectors diametrically opposed in the Injection and Instrumentation module have been replaced with a similar pair of injectors with three tiny orifices inclined towards each other so the three streams impinge 0.8 inch from their exit. This simple modification should help atomize the seed into smaller droplets, thus aiding the vaporization process.

A recent careful inspection of all burner subassemblies revealed that the seats in the fuel valves had aged and were not seating tight, thus allowing fuel into the nitrogen purge lines. A discussion with the valve manufacturer led to their recommendation that these valves should be sent to the factory for servicing. In the interim, small check valves have been installed in the nitrogen purge lines.

INSTRUMENTATION

Process Instrumentation

As an aid to the operator, a number of burner performance parameters are abstracted from the on-line data acquisition and computational process and displayed, on command from the operator, on the control panel, a CRT terminal, or printed. Included among the parameters are the oxidizer N_2/O_2 ratio, the combustion equivalence ratio and the plasma total temperature. This almost immediate display of burner parameters allows the operator to guide the variation of plasma conditions very precisely along a pre-planned course. Additionally, programs were written to provide operator feedback of electrode conditions, such as applied voltage and current, presented in pictorial format illustrative of the test configuration.

Any of the display and informational programs can be executed through an on-line monitor program that provides literally "push-button" operation of the computer.

Experiment Instrumentation

Each experiment has its own instrumentation requirements, and their uniquenesses are reported under the individual experiment headings. This section reports on general experiment-oriented facility instrumentation.

Gas Chromatograph

An actuating mechanism for the insertion of the water-cooled probe into the plasma stream was built and one of the window ports was modified to accept the equipment.

Electrode and Plasma Temperatures

Reference F-1 describes the possible application of an optical multi-channel analyzer (OMA-II) in the direct measurement of the spectral properties of the plasma and, in the video mode with use of a spectral filter, it could function as an optical pyrometer.

Motivated more recently by a recommendation from the Safety Office, two methods have been considered to remotely measure plasma and electrode temperatures.

Laser Line Reversal Method

Dr. Jerome Vigil of the Energy Laboratory has devised a variation of the classical line reversal method for measuring flame temperatures. The main difference between this and the classical system is the replacement of the conventional tungsten lamp with a modulated He:Ne laser as illustrated in Figure F-1.

The basic principle of operation is essentially the same as the classic experiment. As the laser changes (modulates) its output intensity, the signal from the differential amplifier will go through a zero point. At this laser intensity the amount of light emitted by the flame in the laser line bandwidth equals that laser intensity value. This value represents the line reversal point, since

$$N_{pL}(\lambda, T_{pL}) = \epsilon_p(\lambda, T_p) N_b(\lambda, T_p) + \alpha_p(\lambda, T_p) N_L(\lambda, T_L)$$

$$\alpha_p(\lambda, T_p) = 1 - \epsilon_p(\lambda, T_p)$$

implying that, at reversal

$$N_{pL}(\lambda, T_{pL}) = N_L(\lambda, T_L), \text{ or}$$
$$T_p \equiv T_L$$

where

N = spectral radiance

ϵ = emissivity

p = plasma

L = Laser

The effective laser temperature is determined by calibrating the laser output to a tungsten lamp output whose temperature can be measured with an optical pyrometer.

This laser line reversal device would satisfy the requirements for automation and remote operation. The technique requires a highly sensitive photomultiplier tube to detect the radiation from the flame within the bandwidth of the narrow band optical fiber.

Two Color Pyrometer and Electrode Visualization

The principle of two color pyrometry lends itself quite well to the measurement of electrode surface temperature.

Assuming essentially constant emissivity from the electrode surface at two closely spaced wavelengths, the radiation equations yield

$$\begin{aligned} N_1 &= N_E(\lambda_1, T_E) = \epsilon_E(\lambda_1, T_E) N_b(\lambda_1, T_E) \\ N_2 &= N_E(\lambda_2, T_E) = \epsilon_E(\lambda_2, T_E) N_b(\lambda_2, T_E), \text{ or} \end{aligned}$$

$$\frac{N_1}{N_2} = \frac{\frac{C_2}{\lambda_2^5 (e^{\frac{C_2}{\lambda_2 T_P}} - 1)}}{\frac{C_2}{\lambda_1^5 (e^{\frac{C_2}{\lambda_1 T_P}} - 1)}}$$

$$T_E = \frac{\frac{hc}{k} \left(\frac{1}{\lambda_2} - \frac{1}{\lambda_1} \right)}{\ln \frac{N_1}{N_2} \left(\frac{\lambda_2}{\lambda_1} \right)^5}$$

where N_b = Planck Function

$$C_2 = \frac{hc}{k}$$

The choices of wavelength λ_1 and λ_2 are governed by the criterion

$$N_E (\lambda_{1,2}, T_E) \gg N_p (\lambda_{1,2}, T_p).$$

The equipment layout is illustrated in Figure F-2.

The narrow band filter system selects a filter on a filter wheel by operator or computer command. At least three filters would be needed: two for the two color pyrometry, and one for the electrode visualization.

The system would also satisfy the requirements for automation and remote operation.

DATA ACQUISITION

The data acquisition capability utilizing the Energy Laboratory's PDP 11/60 computer and ancilliary equipment which is available to this facility is described in Reference F-1. The use of the line printer graphics in support of post-test data analysis is illustrated under the three major test series reported in the following sections.

FACILITY OPERATIONS

The burner was completely overhauled and long endurance tests were successfully undertaken.

The sampling probe for the collection of gas chromatograph specimens was tried on actual test No. 261 to gain experience with the equipment and identify the proper procedures. The result of the analysis of the specimen taken during Test No. 261 is illustrated in Figure F-3.

Testing during the reporting period, except for facility development tests, was in three distinct series: for Electrode Development; for Friction and Heat Transfer Coefficients Correlation; and on Diffused and Arcing Current Transfer Phenomena. Each category of tests is summarized in separate respective sections.

A list of all tests performed appears in Table F-1 indicating representative test parameters.

ELECTRODE DEVELOPMENT TESTING

Some of the electrode tests were short and for purely thermal integrity qualification purposes to select electrodes that were to be subjected to long endurance tests later. Experience had shown that some electrodes manufactured under identical conditions failed in the warm-up phase of a real test while others did not. Failure of an electrode affects the performance of the other electrodes in the test. It is for this reason that thermal integrity qualification tests have been undertaken to eliminate from the test series those electrodes that are likely to fail early.

Two tests on super-hot electrodes were performed. Test No. 274 was for 17.6 hours duration at a current density of 0.5 amp/cm^2 , and Test No. 276-7-8 was for 13.25 hours at a current density of 1 amp/cm^2 . The test conditions are listed in Table F-1.

Test No. 274, originally planned for three 7-hour installments on consecutive days, was prolonged during the test to a target uninterrupted time of 20 hours, based on the excellent performance that was being experienced. The facility personnel and the logistics for the re-supply of consumables had been arranged to permit extending the initial time if feasible and desirable. After the 17th hour one of the water cooling hoses in the line of sight of the plasma finally burst open, damaged by the prolonged radiant heat to which it was exposed. This condition was corrected for subsequent tests with the use of thermal protection in the form of fire sleeving of 3M manufacture on all the water hoses.

Test No. 276-7-8 for a cumulative time of 13.25 hours was interrupted twice for safety related reasons and re-started immediately after. This

test had been planned for a total duration of 70 hours. The personnel and logistics for this long test were very thoroughly planned. The test was terminated after the 13th hour in an emergency shut-down when the scrubber fan failed, causing an overheating condition in the test section.

Test Performance Analysis

The performance of Test 274 is described here as an illustration of the conduct of the test. Graphs F-4, F-5 and F-6 provide a record of the test conditions for the first 16 hours of Run 274. The missing links in the traces were caused by computer hardware problems subsequently repaired by the manufacturer. The air flow trace (crosses) shows the periodic cycling of the supply air compressor, and the small dips occurring approximately every 25 minutes in the fuel flow trace (circles) reveal the momentary pressurization of the fuel supply twin tank. The large drop in the oxygen flow and the corresponding increase in the air flow at approximately 20:00:00 diurnal hours indicates the on-line refilling of the liquid oxygen supply tank. The instantaneous weight of the seed supply tank (exes) depicts the seed usage during the test. The seed weight has been scaled by a factor of 0.001 to allow plotting in the same graph with the other burner inputs. Note the repeated refilling periods.

The electrode voltage required to maintain a constant current density provides an important clue to the condition of the electrodes during the test. The voltage and current of electrode pair No. 4 are depicted in Graphs F-7 and F-8 and reflect the above phenomenon in the cross voltage trace (circles). The voltage rose in the first few hours of the test, and then the rate of the rise diminished as the electrode surface deterioration reached a certain equilibrium. This phenomenon, if representative, might

possibly provide a direct indication of electrode surface conditions in real MHD channels.

FRICITION COEFFICIENT AND HEAT TRANSFER CORRELATION

The tests under this heading relate to the measurement of convective and radiative heat transfer rates and the correlation with calculated skin friction coefficient in high temperature, non-adiabatic, turbulent flow.

For this purpose, a water-cooled copper rectangular channel 1.65 x 3 x 15" long shown in Fig F-9 was built, and interchangeable boundary layer and heat transfer instrumentation was developed, calibrated and compared with applicable calibrations in colder compressible and incompressible flows.

Interchangeable instrumentation permitted taking heat and pressure data at exactly the same locations by merely replacing the instrumented plugs. The channel had several permanent static orifices along the length of it on both wide walls and several larger ports for the plugs along the channel, also on both wide walls, to receive either heat transfer plugs (Fig F-10) or plugs with one of three diameter (1.8, 2.6 and 3.1 mm D) Preston tubes (Fig F-11). The Preston tube and heat transfer plugs were variously switched to different locations on the walls, and data was taken at four or five different flow conditions for each test configuration. One heat transfer probe was kept at a single location through the series of tests and used as a comparator. All instrumentation plugs had an active water bath for cooling (Fig F-12). Unique among the instrumentation was a 0.095" OD water cooled steel total pressure traverse probe (Figs F-13, F-14) near the downstream end of the channel. This double tube had a total pressure orifice about 0.015" D on the side facing the stream to survey the boundary layer for correlation with the boundary layer pressure profiles derived from the Preston tube measurements. Preston tubes are surface pitot tubes of various characteristic dimensions.

The traverse probe was motorized and geared for very slow travel. Two potentiometers relayed position information to the on-line data acquisition system.

The burner was fueled with JP-4 and the flame temperature was varied by changing the N_2/O_2 ratio in the oxidizer at nearly stoichiometric combustion.

The test apparatus was installed at the exit of the MHD burner, and the test data was taken under conditions that covered the range of temperatures from 1400 to 2700°K, Mach number from 0.4 to 0.7, Re_∞ from $(1.1 \text{ to } 3) \times 10^5$ at heat fluxes up to 180 w/cm². The surface temperature varied from 350 to 450°K. One test was made with a heat transfer gauge in sight of, but not in contact with, the plasma to measure the radiative heat flux.

The relationship sought after (Pseudo Reynolds Analogy) should allow the prediction of fairly accurate skin friction and quite accurate heat transfer coefficients with the use of simple heat transfer gauges by the possible application of Sommer's reference temperature (Ref F-2) or the reference state of zero energy for reacting flows or some other correlation parameter. A slightly modified version of the relationship for "Reference Temperature" suggested by Sommer and Short (Ref. F-2) was used in the calculation of Re' and C'_f , and a similar relationship for "Reference Enthalpy" was used in the calculation of Re^* and C^*_h which are plotted in Figure F-15.

Preliminary analysis (Ref F-3) of the data suggests that the calibration of the Preston tubes in the tested environment is valid, that the flows in these boundary layers can be considered as being frozen (non-reacting), the scatter in the friction coefficient C'_f is acceptable, the

scatter in the heat transfer coefficient is very small and that C_h^* is approximately equal to $0.0469 \text{ Re}^{*-0.2}$ and C_f' is approximately equal to $0.08 \text{ Re}'^{-0.25}$ in the range $2.4 \times 10^5 < \text{Re}_\alpha < 6.8 \times 10^5$, hence $C_f' \approx C_h^*$. See Figure F-15.

Test Performance Analysis

Graph F-16 depicts the test conditions of the MHD burner with respect to the basic input reactants for Test No. 260. The top trace shows the flow of the JP-4 fuel in lbs/sec (x 5 to allow plotting in the same graph with the air and the oxygen flows). The traces marked with triangles and crosses correspond to the pressure differential at the venturi meters for oxygen and air respectively. This is later converted to flow rates.

Two important measurements in the experiments were the traverse location and pressure for the traversing pressure probe, the outputs for which are shown in Graph F-17. The pressure at the probe orifice (Trace with circles) and the probe position are superimposed to portray the performance of the device during the test. The pressure was plotted x 1/5 to make it fit in the same graph scale. The probe position was plotted in volts and provided a relative indication of distance into the channel. The position was calibrated in volts.

The traverses were performed in discrete steps into and out of the stream. The graph portrays three complete traverses and the corresponding pressure traces clearly show, for example, the moment when the probe became plugged (Approx 15:22:00). A nitrogen purge of the pressure line at the probe's outer-most position is recorded as small spikes in the pressure trace.

DIFFUSED AND ARCING CURRENT TRANSFER TESTS

As a basic tool for the experimental program on arcing phenomena in MHD generators, a modular test section was constructed for use in the MHD facility. This test section, with appropriate modifications, is being used for the experiments relating to: a) diffused discharge behavior; b) conditions for transition to arc mode; and c) axial breakdown. The basic construction, shown schematically in Figure F-19, is a brick-lined, copper channel with easily changeable electrode modules to accommodate the different electrode configurations.

Channel Construction

The channel consists of a 3/8" thick case (10" x 6" x 4" outer dimensions) inner lined with a double layer of MgO firebrick, leaving a flow area of 1.75" x 3/75". The surface temperature of the brick is measured by Pt - Pt/Rh 10% thermocouples embedded near the hot surface. The brick thicknesses were chosen such that the surface operates at approximately 2200°K. Water cooling for the channel is provided by 1/4" copper refrigeration tubing, silver-soldered to the copper shell. This simple construction technique is utilized so that modifications necessary to accommodate the various experiments can be easily made.

Electrode Modules

The channel was first configured for transverse discharge and transition to arc mode experiments. The electrode modules, each containing three electrodes separated by insulators, were installed in the narrow wall of the channel, approximately 5" downstream of the channel entrance. The

outer two electrodes served to guard the middle electrode, and each is made of 304 SS blocks (2" x 11/16" x 1/2") brazed onto copper blocks through which cooling water flows. The three electrodes were mounted on a G-10 backing piece and were electrically insulated from each other by 1/16" thick, 96% recrystallized alumina plates.

The rear side of the stainless steel cap of each electrode was slotted to increase the thermal resistance to the cooled copper base. The depth of the grooves increased with the downstream position of the electrode resulting in more uniform surface temperature for all the electrodes. The surface temperature of the electrodes was measured with Cr-Al thermocouples embedded near the hot surface.

Future experiments will employ various surface coatings (platinum, for example) to investigate the effects of surface material on the discharge.

Preliminary design and bench testing of a variable surface temperature electrode has also been completed. This electrode, which uses heating wires to alter the electrode surface temperature, would allow a variation of surface temperature of 200 to 300°K to be studied in a single experiment.

Voltage Probes and Other Instrumentation

The initial experiment was designed to measure the discharge characteristics in the channel and also to measure the electrical conductivity in the core of the flow. For this purpose, the channel was equipped with four arrays of voltage probes. The voltage probes consisted of 20 mil, Pt/Rh 10% wire, encased in 1/16" OD alumina tubing. These probes were inserted into the gas at various levels across the channel through holes drilled in the copper shell and brick liner. Eleven voltage measurements were made

across the channel at the test electrode station and seven across each of the guard electrode pairs.

In addition, a pressure tap measured the local pressure 1" above the channel entrance. A line reversal technique was used to measure the plasma temperature at the channel exit.

Visual observation of the electrode-region was also provided; a special mirror mount was designed and constructed to give a direct view of the electrodes on one side of the channel. A stream of air passing over the mirror kept it relatively free of dust and seed at the beginning of the test.

Videotape and super-8 mm color movies were used to establish the mode of current transport near the electrodes.

Data Collection

The test data was continuously converted to digital form and stored on disk and magnetic tape by the PDP 11/60 computer. A flexible data display program was written and interfaced with the main sampling routines to allow constant real time monitoring of key parameters during the experiment.

Two tests, No.s 263 and 264, were performed with this electrode configuration No. 1, preceded by another test, No. 262, to qualify the brick design. The results from these tests are described in detail elsewhere in this report.

Test Performance Analysis

Graph F-20 illustrates the burner parameters during Test 264. Graph F-21 shows the operating electrical environment imposed by the motor

generator total voltage (exes) and electrode-pair faraday voltages. The electrodes were switched on in a sequence of voltage increments and the boundary voltages and electrode currents observed.

Faraday voltages were generally lower than the M-G supply voltage due to the current-limiting resistances in the electrode circuitry.

Graphs F-22, F-23, F-24, F-25 and F-26 represent the electrical behavior on the "B" portion of the electrical test conditions, from about 19:25:00 to 19:35:00.

Graph F-22 is an expanded plot of the faraday voltages during the test portion B. In general, the center electrode (or test electrode) experienced a voltage about 10% higher than the guard electrodes. The corresponding electrode currents are plotted in Graph F-23 and re-plotted in Graph F-24 with zero reference shifts for easier identification. The bottom three traces display the cathode currents and the top three the anode currents. The excessive currents in electrode pair 5-6 at 19:29:00 were due to the non-simultaneous activation of electrode circuitry.

In similar fashion, Graphs F-25 and F-26 show the voltage differences along the channel across electrode pairs. The voltage probes were arranged across the channel at fixed intervals and were displaced in the plots in the same order.

The primary features to note are the relatively large voltage drops indicated by BB10-BB11 and B1 to BB2, both corresponding to the difference between an electrode potential and the first adjacent probe. These large voltage differences across the boundary layer are discussed in detail elsewhere in this report under the analysis of test results.

DESIGN OF RECONFIGURED FUEL AND OXIDER SUPPLIES WITH AUTOMATIC CONTROLS

Work on this task has been delayed awaiting final definition of Channel 2 characteristics. As it appears at present, Channel 2 will likely have a gas-fired burner, while Channel 1 is liquid fueled. Consequently, reconfiguration of the flow train, including incorporation of automatic controls, remains in suspense until after the desirable characteristics for Channel 2 have been more definitively developed.

DESIGN OF CHANNEL 2

Subatmospheric MHD Generator

The principal specifications for this facility were established and the detailed design of key components and systems was started. The principal specifications are given in Table F-2. It will be noted that these are chosen to match those of an advanced full-scale linear MHD channel.

This facility is to be located in the same explosion-proof test cell as the existing MIT Small Scale MHD Simulation Facility and has many subsystems in common with that facility. The air, oxygen, liquid fuel, seed, coolant and exhaust scrubber systems are in common, and the control systems share most components. The principal infrastructure additions required for the new facility are the piping in of intermediate-pressure (2 atm) natural gas and provision of a steam ejector on the exhaust side to provide for the subatmospheric pressure level required. Fairly minor changes to the test cell, such as the provision of an additional observation window, will also be required. At this time, the question remains unresolved as to which magnet and associated power supply should be used. The available magnet is marginal as to size and field strength (pole face 0.06 x 0.46 m, gap 0.07 m, field 1.2 T), and an alternative based on surplus equipment is being sought. The budgetary constraints of this program preclude the acquisition of a fully-suitable new unit. The available 1 MW DC power supply is being refurbished at MIT expense and negotiations are underway with Westinghouse for transfer to MIT of the 1.5 MW DC power supply formerly at the Waltz Mill MHD facility. For this transfer to be useful, the primary of the transformer will have to be re-wound to accept a 13.8 kV supply rather than

the 4.16 kV for which it is now suited. The decision as to whether or not to proceed with the transfer will depend on the estimated cost of the re-winding, and the moving and re-erection job, as well as the power requirements of the magnet finally selected. A final quotation from Westinghouse is expected shortly.

The conceptual design of the combustor has been through several iterations in order to satisfy the principal requirements listed in Table F-3. The principal variations considered have been fuel type, as between kerosene and natural gas, and wall temperature, as between an ultra-hot ceramic wall liner and a moderately hot stainless steel wall. Each of these variations has many pros and cons, and the oxidizer preheat requirements for them are different.

The principal features of the most current conceptual design (D-1) are listed in Table F-4. This design represents an interesting extreme in the direction of maximizing residence time, and the adequacy of its performance as to plasma conductivity depends on a large degree of preheat as well as an abnormally large seed fraction. If the calculated heat losses are somewhat off, which is not unlikely given the state of the art, an unacceptable large preheat will be required to compensate, as shown in Figure F-27. Thus, in the interests of design conceptualization, the D-2 design is currently being worked on. This shares the principal features of D-1 but is smaller in volume, with a nominal design-point residence time of 35 ms rather than the 55 ms of D-1.

REFERENCES

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F-2 Sommer, S.C. and B.J. Short, "Free Flight Measurements of Turbulent Boundary Layer Skin Friction Coefficient in the Presence of Severe Aerodynamic Heating at Mach Numbers from 2.8 to 7", NACA TN 3391, 1955.

F-3 Gador, A., Private Communication.

Fig. F-1 Laser Line Reversal Layout

RT	EXP RT	AREA	CAL #	AMT PER CENT
3.51	3.52	1272000	3	19.215 O ₂
4.43	4.37	440100	4	6.827 CO ₂
9.61	9.55	5572000	6	74.396 N ₂
11.45	11.41	92	7	0.000 C ₄ H ₄
13.71	13.76	22660	8	0.362 H ₂ O

**Fig. F-3 Gas Chromatograph Results
Specimen from Test No. 261**

Fig. F-4 Test Conditions - Run No. 274

Fig. F-5 Test Conditions - Run No. 274, continued

Fig. F-6 Test Conditions - Run No. 274, continued

Fig. F-7 Voltage and Current - Electrode Pair No. 4 - Run No 274

Fig. F-8 Voltage and Current - Electrode Pair No. 4 - Run No. 274, continued

Fig. F-9 Schematic of Heat Transfer and Boundary Layer Survey Apparatus

Fig. F-10 Heat Transfer Plug

Fig. F-11 Preston Tube

Fig. F-12 Probe Water Cooling

Fig. F-13 Pressure Traverse Probe

Fig. F-14 Pressure Traverse Probe Mechanism

(a) C_h^* vs. R_e^*

(b) C_f' vs. R_e'

Fig. F-15. Heat Transfer and Skin Function Coefficients vs. Reynolds Number for Different Temperatures

Fig. F-16 · Test Conditions - Run No. 260

Fig. F-17 Traverse Probe Travel and Pressure Run No. 260

Fig. F-18 Heat Transfer Data - Run 261 (Thermocouple Reading)

Fig. F-19 Schematic of Enclosed Channel

Fig. F-20 Test Conditions - Run No. 264

Fig. F-21 Electrical Environment - Run No. 264 (Electric Field Intermittently Applied)

Fig. F-22 Electrode Voltages - Run No. 264
(Electric Field Intermittently Applied)

Fig. F-23 Electrode Currents - Run No. 264
(Electric Field Intermittently Applied)

Fig. F-24 Electrode Currents - Run No. 264
(Electric Field Intermittently Applied)
Plotted with Shifted Zero Reference for Easier Identification

Fig. F-25 Voltage Gradients across the Channel - Run No. 264
(Electric Field Intermittently Applied)

Fig. F-26 Voltage Gradients across the Channel - Run No. 264
(Electric Field Intermittently Applied)

Plotted with Shifted Zero Reference for Easier Identification

Figure F-27 Predicted performance of D-1 combustor

TABLE P1

ELECTRODE DEVELOPMENT AND ELECTRO-FLUID MECHANICS TESTS
IN MHD SIMULATION FACILITY

Run Sequence No.	Electrode Stack Nos.	Date	Total Durtn. (h)	Seed Durtn. (h)	Current Durtn. (h)	$\dot{m}$ Total (kg/s)	N_2/O_2	ϕ_{O_2}	Seed (XK)	Slag (kg/s)	Current Density (A/cm ²) max	T_o (K)	P_o (atm)	Mach No.	Q_{hot} (W/cm ²)	Q_{cold} (W/cm ²)	σ (mho/m)	Remarks
260	Test	12/14	1.00			.251	.387	.954				2440	1.262	.653				Friction Coef.
261	Test	12/27	1.00	.05		.253	.350	.911	1.01			2467	1.244	.629				Friction Coef.
262	Test	1/4/79	.50			.248	.347	.890				2497	1.283	.677				Brick thermal test
263	AC 1	1/30	1.25	.60	.40	.192	.866	.944	.876		1	2164	1.165	.567			[.29]	Arcing
264	AC 1	2/1	2.06	1.85	[1]	.215	.748	.853	1.05		1	2092	1.253	.657			[.11]	Arcing
270	243-244	4/2	.1			.171	.899	.744				1341	1.061	.309				Thermal Integrity
271	245-246	4/3	.1			.131	1.49	.498				535	1.041	.253				Thermal Integrity
272	251-252	4/3	.1			.130	1.54	.682				824	1.001	.167				Thermal Integrity
273	250-249	4/4	.06			.134	1.39	.651				755	1.011	.163				Thermal Integrity
274	248A-247C	4/10	17.6	17.08	17.0	.251	.434	1.002	1.13		.5	2855	1.242	.672	168.1	220.4	22.09	Endurance
276-7-8	253A-254C	5/3	13.25	12.02	11.88	.246	.495	1.008	1.25		1	2850	1.116	.475	161.8	243.1	14.59	Endurance

NOTE: 1) Burner and facility modification tests are not included

2) Estimated values are in square brackets

Table F-2

Key Specifications for Subatmospheric MHD Facility

Mass Flow	0.35 kg/s
Plasma Conductivity (at T_0)	10 - 20 mho/m
($\omega\tau$)	5+
Channel Pressure Ratio	5+
Backpressure (diffuser)	0.2 atm (abs.)
Magnetic Field Length (axial)	0.5 m (min.)

Table F-3

Key Combustor Requirements

Mass Flow	0.35 kg/s
Plasma Conductivity (at T_o)	10 - 20 mho/m
Stagnation Pressure	0.8 - 1.2 atm (abs.)
Residence time	30 ms minimum
No seed species condensation on walls	
Maximal plasma uniformity	
Low turbulence exhaust	
Minimal warmup time	
Hard start capability	
Crash shut-down capability	

Table F-4

Key Features of D-1 Combustor Conceptual Design

Fuel	Natural gas
Oxidizer	Oxygen-air mixture $N_2/O_2 = 0.0 - 0.5$
Oxidizer Preheat	1300 K maximum
Seeding	KOH/water 45% solution sprayed by "Sonicore" nozzles into hot- wall (1000 K) oxidizer plenum
Injection System	Multiple (50+) doublet shower- head
Ignition	H.E. spark ignitor
Flow Type	Plug
Wall Material	Stainless Steel, SS-316, 18 mm mean thickness, water-cooled
Mean Wall Temp. (norm.)	1100 K
Length (excl. exit cone)	0.5 m
Diameter	0.6 m
Volume	0.155 m^3
<u>Design Point Operation:</u>	
P_o	1.0 atm
N_2/O_2 ratio	0.0
preheat power	279 kW
seed frac.	1.0 %
thermal power	3.9 MW
T_o	2771 K
σ	10.2 mho/m
T_{wall}	1114 K
heat loss	34.3 %
res. time	55.0 ms

TASK 4, COAL COMBUSTION STUDIES

TASK 4a, KINETIC MODELLING OF 2-STAGE COMBUSTOR

Devolatilization Studies

Soot formation may have biased previous measurements of volatile yields from coal pyrolysis since any soot resulting from secondary reactions was collected along with the char. Thus, recent efforts have been concentrated on developing a technique to determine the relative amounts of soot and char produced in coal pyrolysis. Numerous methods have been investigated (See 1976-1978 Summary Report). The most successful approach has been aerodynamic separation using a cascade impactor. Results show that the small particle fraction may contain condensed tars and condensed ash in addition to the expected soot. However, the impactor presented several problems. The quench system had to be modified since water was no longer an acceptable quench fluid, since the products are to be collected and separated. Argon quench has been substituted for the water. Particle separation using an Anderson Impactor was used for preliminary studies. Problems with wall deposition due to thermophoresis were encountered, and reentrainment of particles from the impactor stages. In order to overcome these problems of quench, thermophoresis and sample size, a centripeter and a new collection probe were designed and built to be operated in conjunction with the cascade impactor.

In the centripeter, the particle laden stream is accelerated through a nozzle. A small portion (approximately 10 percent) of the total gas flow is drawn through a collection nozzle located a short distance below the discharge plane of the acceleration nozzle. The larger particles possess sufficient momentum to penetrate the stagnant gas in the collection whereas the small particles follow the streamlines of the bulk gas flow to be collected on a side arm filter. The advantages of the centripeter are that it can handle high gas flowrates (50 ℓ in our application) and it can be adapted to run in a continuous mode for larger size samples. The disadvantages are that it does not provide multiple size cuts and its separation efficiency is somewhat lower than that of the impactor. Multiple stage centripeters are difficult to build and subject to significant wall losses. However, with the bimodal distribution of particle sizes, characteristic of soot and char mixtures, a single stage centripeter should be sufficient for our needs. Due to the gas flow through the center nozzle the large particle fraction always contains some of the small particles. A correction can be made based on this flow. However, in our case the separation efficiency will be enhanced by feeding the reduced flow of the large particle fraction to the cascade impactor.

The centripeter is shown in its assembled and disassembled states in Figure One; its construction material is plexiglass. The present nozzles may be conveniently replaced with nozzles of other sizes if necessary. The separation efficiency of the

Disassembled Centripeter

Assembled Centripeter

FIGURE 1.

centripeter is presently being optimized using mixtures of glass beads and carbon black.

Much of the problem due to thermophoresis is eliminated by the rapid gas quench, which is now possible with the addition of the centripeter. However, to further eliminate the possibility of particle deposition on the collection probe walls, the probe was modified (See Figure Two) to include a steel sintered tube. The sintered tube is located directly downstream of the rapid gas quench and extends to the probe exit. Argon gas is injected radially along the sintered tube at a rate of 6 l/min, thus preventing depositions on the probe walls.

A gas chromatograph is being modified for semi-continuous analysis of hydrogen gas. Techniques for analyzing tars are also being explored.

Equilibrium Calculations for Fuel-Rich Conditions

The char consumption step is the rate limiting process in coal combustion. Under fuel-rich conditions, char oxidizing species other than O_2 can be present in large concentrations. The possible reaction species include O_2 , CO_2 , H_2O , O , and OH . Their reactions with the char are represented by the following equations:

FIGURE 2
COLLECTION PROBE SCHEMATIC

At the high temperatures of interest in MHD combustors, the reactions occurring in the gas phase are much faster than the rate of the char gasification reactions shown above. Thus, the various char oxidizing species will be present in the gas phase at concentrations at or near their equilibrium values.

Equilibrium calculations were performed for Montana Rosebud (subbituminous) coal, whose elemental composition is shown in Table 1 below.

Table 1- Elemental Composition of Montana Rosebud (d.a.f. basis)

<u>Element</u>	<u>C</u>	<u>H</u>	<u>O</u>	<u>N</u>	<u>S</u>
<u>Mole Percent</u>	46.62	41.74	10.80	0.58	0.26
<u>Weight Percent</u>	74.97	4.97	17.84	1.09	1.13

Equilibrium of the ash-free coal with 5% water by weight in the presence of various amounts of air was calculated assuming the possible presence of C(s) as well as the gas species shown in Table 2. These include many atomic and radical species known to exist in hot combustion gases^{T1}.

Table 2 - Gaseous Species Considered in Equilibrium Calculations.

CO ₂	Ar	CO	C ₂ H ₂	HCN	NH	S
H ₂ O	C	COS	C ₂ H ₄	H ₂	NH ₃	SO
O ₂	CH	CS	C ₂ H ₆	H ₂ O ₂	NO	SO ₂
O	CH ₄	CS ₂	C ₂ N ₂	H ₂ S	N ₂	S ₂
OH	CN	C ₂	H	N	N ₂ O	

The computations were based on the technique of minimization of the Gibbs free energy of the species present, whose details are not given here.

The results are presented graphically in Figures 3 - 10, giving the mole fractions of CO_2 , H_2O , O_2 , O and OH as a function of fuel/air equivalence ratio in the range 1 to 4. Figures 3-6 show the results for 1 atmosphere total pressure at temperatures of 1500, 2000, 2500, and 3000°K, respectively. Figures 7 -10 show results at these same temperature levels at 5 atmospheres total pressure.

The features of the equilibrium curves include:

- . CO_2 and H_2O are always present in similar concentrations.
- . Ratios of CO_2 or H_2O to O_2 exceed 1000 at fuel equivalence ratios greater than 2 and temperatures less than 2750°K.
- . OH is appreciable but does not become important until temperatures exceed 2500°K.
- . O atoms are minor species compared to OH except when the temperature approaches 3000°K.
- . OH is present in concentrations greater than O_2 except at fuel equivalence ratios very close to 1 (stoichiometric).
- . Solid carbon is an appreciable equilibrium product at fuel equivalence ratios greater than 2.5, causing the concentrations of the 5 char oxidizing species to fall to lower values.
- . The curves for 1 and 5 atmospheres total pressure show both similar trends and similar quantities for all 5 char oxidizing species considered.

FIGURE 3.

FIGURE 4.

FIGURE 5.

MOLE FRACTION

FIGURE 6.

FIGURE 7.

FIGURE 8.

FIGURE 9.

FIGURE 10.

These equilibrium data have major implications concerning the relative importance of O_2 , CO_2 , H_2O , O and OH in char burnout under fuel-rich conditions. The major point to observe is that O_2 will not be the major char oxidizing species under all conditions since its concentration can be considerably lower than those of CO_2 , H_2O , and OH , especially during the final stages of char burnout. A more complete assessment of the relative reactivities during char burnout must necessarily be based on the kinetics of the individual reactions and a consideration of mass and heat transfer effects.

CHAR OXIDATION AND ASH VAPORIATION

Vaporization kinetics of the mineral constituents over a temperature range of 2000 to 3000°K were obtained in conjunction with a parallel EPRI sponsored program. The data have been utilized to predict the vaporization of ash in an MHD combustor. The results are summarized in the paper to be presented at the 18th Symposium on Engineering Aspects of Magnetohydrodynamics, appended to this section.

TASK 4, COAL COMBUSTION STUDIES

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KINETICS OF CHAR BURNOUT AND ASH VAPORIZATION
IN COAL-FIRED MHD COMBUSTORS

Appendix to Task 4a.

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Abstract

We have assembled a computer model which simulates coal combustion in a simple MHD combustor. Data from parallel experimental work on devolatilization, char oxidation, and ash vaporization at MHD conditions provide input parameters to the model and provide checks on some of the assumptions therein. We show trade-offs between ash vaporization and char utilization predicted by the model. Finally we indicate from both theoretical and experimental considerations the importance of CO_2 , H_2O , O_2 , O , and OH to char oxidation.

I. Introduction

Experience with conventional coal-fired boilers provides little guidance for the design of MHD combustors since the size of the radiant chamber of a boiler is determined by heat transfer considerations and the peak temperatures in conventional boilers are below the level at which significant ash vaporization occurs. By contrast, the selection of the residence time in an MHD combustor involves a delicate trade-off between carbon combustion efficiency and ash vaporization. This paper presents models for predicting the rates of carbon burning and ash vaporization and uses these to provide a quantitative measure of the effect of residence time on the performance of an aerodynamically simple model of a combustor.

Several models¹⁻³ have been developed for predicting the efficiency of burnout of coal in a combustor operated at the conditions of interest to open-cycle MHD power plants. The model development has been coupled with an experimental program to test the validity of critical assumptions in the models or pertinent kinetic data. The present paper follows such an approach. Inasmuch as the rate controlling step in the combustion of a coal particle is that of the char residue, the first section of this paper presents data on the effect of devolatilization temperature on the fraction of coal converted to char. The second section provides data on the combustion times of coal particles in oxidizing atmospheres at temperatures up to 3000°K in order to test the validity of the assumption that the oxidation rate of char at high temperatures is diffusion-limited and that CO is the primary reaction product. Data on the vaporization rates of the major ash ingredients are presented next and then incorporated with the coal burning model to predict the variation with time in a plug flow reactor of the char burnout and the extent of vaporization of the ash constituents. The predictions are first obtained assuming O_2 to be the dominant oxidant for char. Reactions of carbon with CO_2 , H_2O , and to a lesser extent O and OH are expected to become important at high temperatures, particularly under fuel rich conditions. There is controversy concerning the importance of these reactions. Gray and Kimber⁴ measured the rate of reactions of charcoal with CO_2 and H_2O for temperatures up to 2800°K and concluded that about 90 percent of the rates they measured was attributable to oxygen

produced by the dissociation of CO_2 and H_2O at high temperatures. Because of the dominance of the O_2 contribution to the carbon gasification they were unable to obtain reliable estimates for the kinetics of the $\text{CO}_2\text{-C}$ and $\text{H}_2\text{O-C}$ reactions. Stickler and Ubhayakar⁵ in studies of carbon combustion over a range of fuel/air ratios concluded that the endothermic $\text{CO}_2\text{-C}$ and $\text{H}_2\text{O-C}$ reactions were dominant under fuel rich conditions since the particle temperatures were lower than the calculated gas temperatures. They were unable, however, to obtain reliable estimates of the rate constants because of the large fluctuations in their measured temperatures. The final sections of the present paper provide additional data on the $\text{CO}_2\text{-C}$ and $\text{H}_2\text{O-C}$ reactions and used these to provide additional insight on the importance of these reactions in reducing the time requirements for combustion under fuel rich conditions.

II. Devolatilization

Coal particles injected into a combustion chamber are rapidly heated at rates of about 10^5 to 10^6°K/sec by radiation and the recirculation of hot combustion products. Volatiles are released rapidly and may burn heterogeneously at the particle surface or as a volatile flame depending on mixing conditions. Although volatile and char combustion may occur contemporaneously, the rate of volatile release is sufficiently more rapid than that of the char consumption that, for modeling purposes, coal combustion can be considered in two sequential stages: rapid evolution of volatile matter and the subsequent slower oxidation of the char. The devolatilization process can affect char oxidation in two obvious ways: (1) the amount of volatiles produced determines how much char remains to burn and (2) physical changes, especially in size, will alter the mass transfer rates to and in the char particles.

Volatile yields. It is well known that heating the coal particles in a dispersed flow to the high temperatures encountered in MHD combustors can increase volatile yields up to twice the values obtained in the ASTM proximate analysis. Volatile yields of 60 to 80 weight percent are not uncommon for a coal that yields only 40 percent volatiles in the standard test procedure. The implications of enhanced volatile yields are significant since an increase in volatile yield from 40 to 80 weight percent results in a three-fold decrease in the char left to be burned.

In view of the complexity of the physical and chemical processes influencing the release and transport of volatiles from heated coal particles⁵⁻¹¹ reliance has been placed on experiment for the determination of volatile yields. Results on volatile yields for temperatures up to 2100°K have been reported previously based on the measurement of the residue obtained when size-graded coal particles were heated for known time intervals along the center axis of an electrically heated laminar flow furnace¹². Interpretation of

the data and those of parallel studies by Ubhayakar et al.¹³ were complicated by the formation of soot by the secondary pyrolysis of hydrocarbons in the devolatilization products. The soot formed was collected with the char in the bronze filter used to collect the quenched solid products of the pyrolysis reactions in Reference 12 and was inferred from the gas phase composition of the pyrolysis products in Reference 13. Uncertainties in the estimation of the amount of soot formed were eliminated in the present study by passing the quenched pyrolysis products through a cascade impactor in order to separate the soot and char. The effectiveness of the separation achieved can be noted from the photographs of the filters from the six stages used in the impactor. These show that the char is confined mainly to the precollector and zeroth stage of the impactor and that the soot is deposited primarily on the final filter and very little material in between (See Figure 1). Aerosols produced by the vaporization and recondensation of the mineral matter in coal or by the condensation of tars were also found to be collected on the final filter. The soot-free char residue obtained for a Montana Rosebud and Montana lignite heated to temperatures of 1250 to 2500°K for one second in argon was used to obtain the volatile yields reported, on a dry ash free basis (See Figure 2). A very weak increase, if any, is noted in the volatile yield at temperatures above 1750°K.

Figure 2. Volatile yields (dry, ash free basis) for Montana lignite and Montana Rosebud in argon at 1 second residence time.

Figure 1. Photograph of material deposited on stages of cascade impactor, from pyrolysis of Montana Rosebud at 2000°K.

Particle Size Changes During Pyrolysis.

In order to determine the effect of devolatilization on particle size, physical changes during devolatilization of pulverized particles of Pittsburgh Seam #8 hVA coal (mean size 57.5 μ m) and Montana lignite (47.6 μ m) were monitored at temperatures between 1000 and 2100°K. Reaction times ranged from one second down to a few milliseconds at heating rates from 10^4 to 10^6 °K/sec in argon. Tests were made for three types of furnace conditions: (1) laminar flow to rapid quench, (2) free fall to rapid quench, and (3) free fall to hot collector. The distribution of particle sizes were obtained from analysis of SEM pictures of representative samples.

The results are summarized in Figure 3. Only small changes in average particle size and standard deviation were observed for the lignite. Most of the lignite particles retained their original shapes but exhibited crevices and increased porosity with increasing particle weight loss.

Extensive swelling (to about 500 μ m) was observed for the bituminous coal at temperatures below 1600°K, but no swelling was observed above 1740°K. Agglomeration was also found to be prevalent only below 1500°K. These results are particularly relevant to modeling efforts, since they indicate that at conditions of interest to MHD combustors the assumption that particle swelling is negligible can be justifiably introduced.

III. Char Oxidation Kinetics

The burning rates of coal in nitrogen-oxygen mixtures were obtained by measuring (a) the temperature differential between the surface of the burning particle and the gas, as determined by two-color optical pyrometry, and (b) the tracks of the burning particles viewed through a chopper. The surface temperatures of particles burning in oxidant mixtures at 850°K increased from about 1800°K at an oxygen partial pressure of 0.2 atmospheres to values bracketing 3100°K in pure oxygen. These results were compared with values calculated assuming that (i) the devolatilization was rapid so that the particles could be modeled as char particles, (ii) the product of the carbon oxidation reaction was CO, (iii) the CO was oxidized at a sufficient distance from the surface so as not to affect the surface temperature, (iv) the reaction rate was controlled by diffusion of oxygen to the surface, and (v) the energy loss from the surface was by convection and blackbody radiation. The curve in Figure 4 calculated using these assumptions fits the data reasonably well. Part of the spread in measured particle temperatures is a consequence of particles being viewed at different stages in their combustion history. The curve in Figure 4 is the calculated time average temperature for an initial particle radius of 42 μ , assuming the particle burns as a

Figure 3. Changes in particle size during devolatilization for Pittsburgh seam bituminous coal and Montana lignite.

Figure 4. Measured particle temperatures of Montana lignite burning in O₂-N₂ mixtures: Solid circles are measured in average temperatures, solid curve is calculated from model of diffusion controlled combustion.

shrinking sphere. The range of temperatures predicted as the particle shrinks during burning is ~300°K at 0.2 atm. oxygen partial pressure and over 1000°K at 1.0 atmosphere. Other factors that will influence particle temperature are the energy requirements for devolatilization and energy feedback to the surface from the combustion of volatiles and CO. The burnout times calculated from the mean values of the measured temperatures (solid triangles) are compared with the values calculated by the model (solid curve) and measured from the length of tracks produced by the burning particles (open squares) in Figure 5. The particle density needed in the calculations was determined from the initial coal density and the volatile yields measured in the pyrolysis experiments. Reasonable agreement is obtained between theory and experiment. Refinements of the model will be made to include volatile and CO oxidation in order to determine the extent to which these reactions are responsible for the residual differences between theory and experiment. In addition, the experiments will be extended to other coals to see if the assumption of diffusion limited char-O₂ reaction rates is generally valid. However, the predictive capability of the model in its present form is deemed to be adequate for use in developing the temperature time history of particles for purposes of estimating ash vaporization.

Figure 5. Burnout times of Montana lignite particles in O_2 - N_2 mixtures.

IV. Ash Vaporization

The vaporization of mineral matter in the MHD combustor influences the thermal behavior in the combustor and poses operating problems for equipment downstream. We have measured the rate of vaporization of mineral matter constituents in two different ways. For a more complete discussion, see Reference 14. In one method the loss of various elements from the chars generated in the pyrolysis experiments is measured. Most of the elements are analyzed by neutron activation analysis. Silicon is determined by atomic absorption. The vaporization rates are calculated from the residence time in the furnace and the fractional loss of each element from the char. We also measured vaporization in combustion environments similar to those detailed in the section on Char Oxidation. Coal was burned in a gas of known O_2 concentration and temperature. A wide range of particle temperatures and burnout times were obtained by varying the conditions. The mineral matter which vaporizes forms a fume which can be quantitatively separated from the rest of the ash by aerodynamic classification in a staged impactor. The fume is then analyzed for elemental composition. For most elements, a good correlation is obtained when the vaporization rate (obtained from the fraction vaporized and burning times) is plotted versus particle temperature. Figure 6 shows the correlations obtained for the five major elements in Montana lignite. The data points for magnesium and aluminum are shown as an indication of the quality of the correlations. These data provide an empirical data base for modeling purposes. A simple model for ash vaporization is included in the combustor model which reproduces the ash vaporization data here. This should be useful for predicting the relative effects of various combustion strategies on a particular coal. The ash behavior will vary from coal to coal and as data accumulates, we hope a more general model can be based on the behavior of the individual inorganic constituents.

Figure 6. Arrhenius plots of vaporization rate coefficients for ash constituents. Solid lines are correlations used in the model. Data points are included for Mg(+) and Al(•).

V. Coal Combustion Model

A coal combustion model has been assembled to simulate coal combustion under MHD conditions. It provides the framework on which data for the various facets of the combustion process can be incorporated and sensitivity of the combustion process to specific parameters can be checked. A brief description follows.

The model intakes spherical coal particles, whose size number densities are specified by the Rosin Rammler distribution. The coal may have an arbitrary composition of carbon, oxygen, hydrogen, sulfur, and ash components. The model assumes an instantaneous devolatilization, computes the changes in density resulting due to swelling, and finally calculates the particle size distribution for the devolatilized coal. Air, with specified composition and preheat temperature, is allowed to mix with the devolatilized coal particles and this starts the combustion process. The coal-air stoichiometry can be varied. The volatiles are assumed to be burned instantaneously.

From this point the user can specify combustion of the char to proceed in plug flow or it can enter a continuous stirred section of specified residence time first. In the stirred section as well as each of the plug flow sections, a heat and mass balance calculation is performed. The char is assumed to burn as shrinking spheres. Gas phase components (CO_2 , H_2O , O_2 , OH , H , H_2) are assumed to be in chemical equilibrium. The heat losses to the walls by both particulate and gas radiation and convection are calculated by some simple modeling of the furnace construction if desired.

The ash vaporization is calculated as a first order rate phenomenon. Currently the ash

constituents do not interact chemically with the other gas phase components but reasonable values of heats of vaporization and heat capacities are supplied in order to model the thermal effects of the ash constituents.

Model Runs

The results of a plug flow calculation are shown in Figure 7. This run was made adiabatically; no heat loss was allowed. Following the recommendation of Reference 4, the char was assumed to react only with O_2 , a point to which we will return later. The time evolution of the gas phase species, the vaporization of the various ash constituents, the temperature, and char loading can be seen. At these temperatures the dissociation of CO_2 is sufficient to keep the O_2 mole fractions relatively high through the burnout.

Figure 8 shows another run at a different stoichiometric ratio in which heat loss to the walls has also been included. The amount of O_2 available to react with the char is decreased

Figure 7. Plug flow simulation of coal combustion with 1600°K air preheat, 6 atmospheres total pressure, and fuel equivalence ratio of 0.95. No heat lost during combustion.

both by the higher fuel equivalence ratio and the decreased dissociation of CO_2 at the lower temperatures. The result is that the burnout time is dramatically increased. The dependence of burnout time and ash vaporization on equivalence ratio is shown in Figure 9 for several combustion conditions. With 1600°K preheat the burnout time is insensitive to the heat loss, partly because the short burnout times allow little heat to be lost ($\sim 4\%$). At the higher fuel equivalence ratios the burnout time increases for the adiabatic case quite dramatically. In the model furnace, 18% of the available heat is lost at 90% char utilization and the burnout time is still much longer due to oxygen starvation at the lower temperatures. With 1400°K air preheat the trend is more pronounced.

The effects on ash vaporization are shown in the same figure. Similar ash vaporization behavior is predicted for the two 1600°K cases. As the fuel equivalence is increased in the model furnace, the effect on ash vaporization of the increased burnout time cancels the effect of the lower temperatures. With less severe (1400°K)

Figure 8. Plug flow simulation of coal combustion with 1600°K air preheat, fuel equivalence ratio 1.1. Flow was through a simulated rectangular furnace.

Figure 9. Predicted burnout times and ash vaporization at 90% char utilization as a function of fuel equivalence ratio. Char kinetics include O_2 only. A=adiabatic, R=model furnace, numbers = preheat, $^{\circ}K$.

air preheat, less ash vaporization is predicted at low fuel equivalence ratios but in the high fuel ratio cases, the effects of longer burnout times overcome those due to lower temperatures. The convergence at the long times is only partially due to advanced vaporization ($>.5$) of the two most volatile ash components (Si and Mg).

VI. Char Reactions with Species Other than Oxygen

The possible contribution of species other than oxygen raises large uncertainties in the model predictions. The gas phase concentrations of O , OH , CO_2 , and H_2O shown in Figures 7 and 8 indicate their possible importance in char oxidation. Equilibrium calculations based on the composition of Montana Rosebud coal with five percent water by weight are shown in Figure 10. The pertinent information from these equilibrium data are:

- CO_2 and H_2O are always present in similar concentrations.
- Ratios of CO_2 or H_2O to O_2 exceed 1000 at fuel equivalence ratios greater than 2 and temperatures less than $2750^{\circ}K$.
- OH is appreciable but does not become important until temperatures approach $3000^{\circ}K$.
- O atoms are minor species compared to OH except when the temperature approaches $3000^{\circ}K$.

Figure 10. Equilibrium concentrations of species important in char oxidation at 5 atmospheres pressure as a function of fuel to air equivalence ratio. (a) $2000^{\circ}K$ (b) $2500^{\circ}K$ (c) $3000^{\circ}K$

The equilibrium data at 1 atmosphere is very similar to the data presented here for five atmospheres.

The main point to observe is that O_2 will not be the major char oxidizing species under all conditions since its concentration can be much lower than those of CO_2 , H_2O , O and OH , especially during the final stages of char burnout. A more complete assessment of the relative reactivities during char burnout must be based on the kinetics of the individual reactions.

O and OH Kinetics. Rosner and Allendorf¹⁵ found the reactivity of oxygen atoms to be 5 to 80 times greater than oxygen molecules in the range 1100 to 2000°K. The OH collision efficiency for carbon atom removal has been estimated by Fenimore and Jones¹⁶ to be about 0.1 for soot in ethylene air flames at temperatures of 1530 to 1890°K, which corresponds to a rate slightly smaller than O atoms at equal concentrations. The reactions of char with these species should therefore be limited by external mass transfer.

Char Reactions with CO₂ and H₂O. Carbon is intrinsically much less reactive towards H₂O and CO₂ than O₂¹⁷. However, if the H₂O and CO₂ reaction rates approach external diffusion control at the high temperatures in an MHD combustor, they could contribute significantly to the char oxidation rate. This is especially true in view of the higher partial pressures of these species during char burnout.

Gasification rates of 100 - 200 mesh (75 - 100 μ m) Montana Rosebud coal char in CO₂ and in H₂O at temperatures up to 2070°K and residence times from 0.25 to 0.5 seconds were measured using a laboratory scale laminar flow reactor. The total pressure was 1 atmosphere, and two different partial pressures of each reactant gas were studied. These studies were undertaken to evaluate gasification rates relevant to the BiGas gasification process under a parallel study for the Phillips Petroleum Company. The apparatus and procedure are described elsewhere¹⁸.

The partly gasified char was water quenched and collected for subsequent weighing in a sintered brass filter housed in a water cooled movable probe. Weight losses were determined at various probe positions at each temperature and partial pressure of oxidizing gas. The corrected conversion data at each condition were plotted against residence time to obtain a "first-order" gasification rate, dw/dwt , where w is the dry, instantaneous, ash-free weight of the char. Both reactions fit first order behavior with respect to reactant concentration reasonably well. The first order rate coefficients are plotted as the data points in Figure 11. The solid lines represent the correlation used for the intrinsic rate of the reactions. The dashed lines are the rates predicted including pore diffusion resistance and the intrinsic rates. The char properties used in these calculations match measured properties.

The foregoing kinetics were incorporated into the combustion model and the contributions of the various reactions were evaluated during the char burnout. Figures 12a and 12b show the relative contributions of all five oxidizing species during combustion for fuel equivalence ratios of 0.9 and 1.1. The contributions of O and OH are not insignificant but are not dominating due to their lower concentrations under these conditions. The contributions of CO₂ and H₂O are predicted to be important for the fuel lean case and dominant for the fuel rich case.

The effect is best appreciated by looking at the influence of these reactions on the burnoff curves. Figure 13 shows the char burnoff curves obtained for the two cases in Figure 12. The burnout times are reduced for both cases but much more for $\phi = 1.1$ so that most of the dependence of burnout time on ϕ is washed out.

Figure 11. Rate coefficients for CO₂ and H₂O-char reactions: Data points are from Ref. 18. Solid curve represents the intrinsic rate, dashed curves are rates predicted by inclusion of pore diffusion resistance.

The O₂ reaction is diffusion controlled, therefore the burnout times should show a large particle size effect. For the same Rosin Rammner exponent (1.5), increasing the mean particle size increases the burnout time by the expected second power law (See Figure 14). If one includes the CO₂ and H₂O reactions a dependence remains but the effect is reduced somewhat because the CO₂ and H₂O reactions are not completely controlled by external mass transfer. Considerations of ash vaporization are also modified. A parametric study is not presented here. We wish only to point out the uncertainties involved.

The CO₂ and H₂O reaction rates are not completely limited by mass transfer to the char particle under these conditions. Therefore the predicted behavior of the model is sensitive to changes in these rates. For Rosebud char we have extrapolated the rates at $T < 2100^\circ\text{K}$ to the region 2500°K and above. This is done with little confidence in view of severely non-Arrhenius behavior exhibited in gas-carbon reactions at these temperatures¹⁹. We believe that the effects predicted here are an upper limit. For other coals the intrinsic reactivity to H₂O and CO₂ could be lower and the combustion profiles changed accordingly. This points to a critical need for kinetic data for char-CO₂ and H₂O reactions at these temperatures.

Figure 12a. Predicted relative contributions of various oxidizing species to char oxidation during char burnout in plug flow reactor. Air preheat temperature 1600°K, fuel equivalence ratio 0.9.

Figure 12b. Same as 12a with fuel equivalence ratio = 1.10.

Figure 13. Char burnout profiles for fuel equivalence ratios of 0.9 (triangles) and 1.1 (circles). Solid curves and symbols assume char- O_2 only. Open symbols and dashed curves include reactions of O_2 , CO_2 , H_2O , O and OH .

Figure 14. Burnout times as a function of particle size, showing effect of $CO_2 + H_2O$ kinetics.

VII. Conclusions

1. We have obtained data relevant to the modeling of coal combustion under MHD conditions.

a) We have measured volatile yields from coal at temperatures up to 2500°K. We have also developed an aerodynamic separator scheme to give volatile yields uncomplicated by soot and other secondary solids.

b) We have determined the char reaction rate with O₂ to be described by external mass transfer control at the temperatures and particle sizes of interest.

c) We have obtained ash vaporization rates in both inert and combustion environments adequate for MHD modeling.

2. We have assembled and run a computer model of an aerodynamically simple MHD combustor.

a) The model can calculate the various trade-offs involved in the combustor, for example, ash vaporization versus char burnout and particle size effects.

b) With O₂ as the only char oxidant, severe penalties both in burnout times and ash vaporization are placed on increasing the fuel equivalence ratio.

c) The reactions of CO₂ and H₂O with char could be dominant in determining the char burnout time. The uncertainty in these reaction rates is the most important gap in the data base for modeling the combustion process.

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TASK 4b, COMBUSTION AERODYNAMICS AND FLUID MECHANICS OF HIGH-SWIRL SECOND STAGE

The first-year effort under this Subtask is concentrated on the development of a large-scale water-flow test model of the proposed swirl combustor configuration and a parallel program of mathematical modeling of this unique combustor's flowfield.

An experimental rig has been designed and constructed which consists of a large octagonal water tank (6' across x 42" high) having clear plexiglas sides, Fig. 4b.1. The water is circulated in a closed loop using a 400-gpm 30-ft head pump.

The primary stage of the combustor consists of a 5" I.D. axial/tangential entry type swirl generator in which tangential fluid is introduced via eight tangential slots and axial fluid via an axial inlet positioned on the central axis of the combustor. This combustor has also been designed and constructed from clear plexiglas, and enables quasi-independent variation of the degree of swirl and the flow rate in the first stage.

The second stage of the combustor is designed and constructed in the form of a multi-annular (three) swirl combustor in which swirl is imparted to the flow by means of 3 sets of 48 aerodynamically-designed blades placed in the upstream section of the flow, Fig. 4b.2. This facility allows variation of the degree of swirl and the flow rate in each of the three annuli independently. The first-cut combustor exit nozzle shape has been designed and constructed based on previous experience. More refined shapes will be substituted as the study progresses.

A high-intensity slit light source has been designed and construction is currently in progress. This will be used for streak photography of suspended SG 1.0 beads. It is expected that useful water model data will first become available in June 1979.

In parallel with the water model experiments, a computer program has been written in Fortran for modeling the test combustor mathematically. The computer program, which is based on an existing program called COSMIC (Computation of Swirl Mixing in Combustors), calculates 2-D steady axisymmetric recirculating swirl flow both without and with chemical reaction and is currently being made to run on the group's PDP 11/60 computer. The program uses the primitive pressure-velocity finite difference technique with a scattered grid system for axial and radial velocities. The original COSMIC code deals mainly with gas-turbine combustors, with emphasis on free and confined flows downstream of a sudden expansion of the axisymmetric cross-section under both isothermal and combustive conditions. The flow downstream of the swirl generator can be a free jet or a premixed flame without or with any degree of swirl. This program is currently being developed for computing the flow within the second stage of the new combustor and will be used (in parallel with the experimental program) to analyze the effects of input variables, e.g. inlet flow velocity and swirl-vane angle setting (swirl number) on the flowfield in the combustor.

Theoretical analysis for the design of the second-stage nozzle so as to turn the flow from the axial to the radial direction aerodynamically without flow separation has also begun. In the first stage of the process a computer program is being written for evaluating the shape of the outer wall, in particular, looking for flow separation for an assumed combustor exit shape and swirl. This is a 2-D program which deals with steady, isothermal non-reacting, high-swirl flow. It employs the vorticity stream function approach. This program is currently being debugged and it is expected that it will be fully operational within the next few weeks. If a separation occurs with the present shape, a new shape will be generated

using the code. In later work, a new program will be written to calculate the velocity profile in the second-stage nozzle. The new program will calculate 2-D, axisymmetric, recirculating swirl flow and will employ the primitive pressure-velocity approach. The mixing of the primary or first-stage fluid with the secondary-stage fluid within the combustor and exit nozzle will also be calculated.

Figure 4b.1. View of water tank prior to swirl-generator and model installation.

Figure 4b.2.
 WATER TEST MODEL OF
 MIT ENERGY LABORATORY HIGH EXIT
 SWIRL TWO-STAGE MHD COMBUSTOR

IV. CONCLUSIONS

TASK 1. ARCING PHENOMENA IN MHD GENERATORS

The principal conclusions relating to discharges to clean (non-slugged) electrodes are as follows:

- 1) No visible evidence of tranverse arcing was observed on stainless steel anodes ($T_{\text{surface}} \sim 1100 \text{ K}$) at current densities as high as $\sim 2.5 \text{ A/cm}^2$. This is at variance with observations of others with different electrode material, but is consistent with previous work at Stanford with stainless steel electrodes.
2. Comparison of predicted and measured anode voltage drops indicate,
 - a) resistance underpredicted if only 3-body electron recombination is included,
 - b) with recombination rates increased such that the low current resistance matches experimental data, the effect of Joule heating on resistance is still underpredicted. Several sources for these discrepancies are identified and are being further evaluated.

The conclusions relating to slag polarization effect reached in this reporting period are:

- 1) The literature data indicate the existence of cumulative slag polarization due to ionic mobility. A steady-state equilibrium polarization state with diffusion balancing field drive seems unlikely over most of the slag layer.
- 2) A 25% conductivity decrease has been measured after nine (9) minutes at about 1 amp/cm^2 at slag temperatures below 1200 K . This indicates ionic conduction even in the solid state.

Preliminary experimental results with a simulated slag flow have been demonstrated the usefulness of this technique. This results showed that ripples, qualitatively similar to those observed in slagged-wall experiments can be obtained in the simulation rig. Since measurements are extremely easy (due to the lack of hot plasma) development of a data base and off analytical models should be greatly speeded over similar activities for actual slagged wall operation.

TASK 2. OPEN CYCLE DISK GENERATOR PROGRAM

1. Prospects for the wake nonuniformity study being successful look good.
Based on the results and analysis of initial reference flow tests, the base case channel configuration, test gas and conditions best suited for the study were defined. Additional experiments are required, however, before tests with wakes can begin.
2. A "spider" of flat-plate profiles (up to 50) will be used to create the radial wakes. Encouraging theoretical results for the wake effect (on the open-circuit voltage reduction) produced by the this design were obtained.
3. The high-bandwidth A/D data aquisition system has been successfully implemented into the experimental program.
4. Progress has been made on the two-dimensional modeling of disk generators.

TASK 3, ELECTRODE MODULE DEVELOPMENT AND TESTING

PART A.

Several general conclusions may be drawn with respect to the two systems studied, $\text{SrZrO}_3\text{-LaFeO}_3\text{-SrFeO}_{3-\delta}$ and $\text{SrZrO}_3\text{-LaFe}_{0.5}\text{Cr}_{0.5}\text{O}_3\text{-SrFe}_{0.5}\text{Cr}_{0.5}\text{O}_{3-\delta}$:

- a. A substitution of $(\text{Fe}_{0.5}\text{Cr}_{0.5})$ for (Fe) in the ternary $\text{SrZrO}_3\text{-LaFeO}_3\text{-SrFeO}_{3-\delta}$ system increases refractoriness, but also increases the size of the 2-phase regions. The electrical conductivity also decreases slightly.
- b. Refractoriness increases, but electrical conductivity decreases as the SrZrO_3 corner of the ternary phase diagram is approached.
- c. On analysis, using a combination of Seebeck voltage and electrical conductivity measurements indicated that at reducing atmospheres near 1 atmosphere, the number of p-type carriers saturate.

Development

MgAl_2O_4 seems to have the best dielectric properties of the four materials tested to date.

Testing

Little additional degradation was observed in single-layer electrodes operated for 17 hours than had been observed in electrodes operated for two to six hours previously.

Multilayered electrodes operated satisfactorily at a current of 1 A/cm^2 for greater than ten hours, but underwent somewhat more degradation than the single-layered electrodes.

PART B.

The MHD Facility Complex

1. Installation of a collar in the experimental tank significantly reduced exhaust gas recirculation and tank overheating.
2. Ceramic electrodes have been operated at $1/2 \text{ amp/cm}^2$ for over 17 hours and at 1 amp/cm^2 for over 13 hours without failure.
3. Performance results from long endurance tests seem to indicate a possible correlation between electrode voltage and surface condition.
4. Preliminary analysis of test results suggests the relationship $C'_f = 1 C^*_h$ for Re_α of $(2.4 \text{ to } 6.8) \times 10^5$ in high temperature, non-adiabatic, turbulent flows.

TASK 4, COAL COMBUSTION STUDIES

TASK 4a, KINETIC MODELLING OF 2-STAGE COMBUSTOR

A method for separating soot and char by aerodynamic means has been developed. This will enable improved determination of volatile yields during pyrolysis, and thus improve the char yield estimates used in the char oxidation calculations. Computations of the char consumption rate showed that the $\text{CO}_2\text{-C}$ and $\text{H}_2\text{O-C}$ reactions had a major effect on burnout times under fuel-rich conditions. Application of the preliminary values on the kinetics of char gasification and ash vaporization have shown that severe constraints exist on the selection of a residence time for an MHD combustor as a consequence of trade-offs between char combustion efficiency and amount of ash vaporized.

TASK 4b, COMBUSTION AERODYNAMICS AND FLUID MECHANICS OF HIGH-SWIRL SECOND

STAGE

No new conclusions are available from this effort at this time.