



A simulation approach to a magneto-hydrodynamic-steam electrical power generation system
by Teodoro Canillas Robles

A thesis submitted in partial fulfillment of the requirements for the degree of DOCTOR OF
PHILOSOPHY in Electrical Engineering

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Abstract:

A mathematical model of a magnetohydrodynamic-steam electrical power generation system was developed, incorporating the results of experimental and theoretical investigations conducted by various researchers, applicable to large power generation. A real gas at sub sonic condition (typical for large power generation) is used in the flow calculation. A quasi one-dimensional MHD generator model was used to explore the advantages of the model in the design of the control mechanisms for the system. The numerical techniques used in the simulation are described and the results of the simulation are presented. The behavior of the MHD generator in response to load changes are discussed. The dynamic model of the direct-current (DC) to alternating-current (AC) power converter is presented and the design parameters compatible with the MHD generator operation are discussed. The dynamic model of the steam plant and air heaters was developed and the results of the simulation of the model in response to changes in input conditions are shown. The design parameters of the steam plant for a 2000 megawatt (thermal input) MHD system are given.

A SIMULATION APPROACH TO A MAGNETOHYDRODYNAMIC-STEAM
ELECTRICAL POWER GENERATION SYSTEM

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TEODORO CANILLAS ROBLES

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TABLE OF CONTENTS

	<u>Page</u>
VITA	ii
ACKNOWLEDGEMENTS	iii
TABLE OF CONTENTS	iv
LIST OF TABLES	vii
LIST OF FIGURES	viii
LIST OF SYMBOLS.	xi
ABSTRACT	xvii
CHAPTER	
I. INTRODUCTION.	1
1.0 System Simulation.	1
1.1 Open-cycle MHD-Steam Electrical Power Generation.	1
1.2 Historical Review.	6
1.3 Statement of the Problem	17
II. FORMULATION OF THE MATHEMATICAL MODEL OF A PRACTICAL MHD GENERATOR.	20
2.0 Introduction	20
2.1 Magnetohydrodynamic Equations.	20
2.2 Magnetohydrodynamic Approximations	24
2.3 Quasi one-dimensional MHD Equations.	27

	<u>Page</u>
2.4 Heat Transfer and Friction Effects	30
2.5 Working Fluid Thermodynamic and Electrical Properties	35
2.6 The Single Load Segmented MHD Generator Model .	37
2.7 Boundary Layer Effects	52
2.8 Scope and Limitations of the Model	55
III. FORMULATION OF THE MATHEMATICAL MODEL OF THE MHD ELECTRICAL POWER GENERATION SYSTEM COMPONENTS . .	57
3.0 Introduction	57
3.1 The Steam Bottoming Plant and Air Heater	57
3.2 Formulation of the Mathematical Model of the Steam Plant and Air Heater	63
3.3 Dynamic Model of the DC to AC Converter.	81
IV. COMPUTER IMPLEMENTATION OF THE SYSTEM MATHEMATICAL MODEL	96
4.1 The MHD Generator and Diffuser	96
4.2 The Steam Bottoming Plant and the Air Heater . .	101
4.3 The DC to AC Converter	104
4.4 Computer Program to Simulate the Mathematical Model	105
V. RESULTS OF THE COMPUTER SIMULATION	107
5.0 Introduction	107
5.1 Steady State Flow in the MHD Generator	107

	<u>Page</u>
5.2 Time-dependent Flow in the MHD Channel.	116
5.3 Dynamic Response of the Steam Plant	129
VI. SUMMARY AND SUGGESTED FUTURE RESEARCH	139
6.0 Introduction	139
6.1 Summary and Conclusions	139
6.2 Suggestions for Future Research	142
APPENDIX	143
A. THERMODYNAMIC AND ELECTRICAL PROPERTIES OF THE WORKING FLUID	144
B. DESIGN PARAMETERS OF THE STEAM PLANT AND AIR HEATERS	149
C. THERMODYNAMIC PROPERTIES OF WATER/STEAM	156
D. THERMODYNAMIC AND TRANSPORT PROPERTIES OF AIR	160
E. SPECIFIC HEAT OF THE COMBUSTION GAS	162
F. CALCULATION OF THE COMBUSTION TEMPERATURE.	164
G. FORTRAN IV COMPUTER PROGRAM	167
REFERENCES	180

LIST OF TABLES

<u>Table</u>	<u>Page</u>
2.1 Expression for the Electrical Parameters of the MHD Generator	47
2.2 Generator Parameters with Segmentation Effects.	49
3.1 Transformer Three Phase Voltages and Line Currents	92
5.1 MHD Generator Design Parameters	109
5.2 Design Parameters of the Steam Plant.	130

LIST OF FIGURES

	<u>Page</u>
<u>Figure</u>	<u>Page</u>
1.1 A Simple MHD Generator	4
1.2 Coordinate System	4
1.3 Open-cycle MHD Electrical Power Generation System .	7
1.4 Three-Phase Bridge Inverter Circuit	13
2.1 Diagram for the Analyses of a Linear MHD Generator .	38
2.2 Continuous Electrode Faraday Generator	38
2.3 Segmented Electrode Faraday Generator	41
2.4 Hall Generator	41
2.5 Series or Cross-connected Generator	44
2.6 Coordinate System for the Segmented Electrode Generator	44
2.7 Correction Factors u , u' , v , v'	51
3.1 MHD-Steam Electrical Power Generation System	58
3.2 Water/Steam Cycle - Steam Bottoming Plant	61
3.3 Gas Path - MHD/Steam Electrical Power Generation System	62
3.4 Sectional View of the Checker Brick Regenerator. . .	75
3.5 Model used in the Numerical Analyses of the Brick Regenerator	78
3.6 Brick Regenerator Matrix Elements Temperature- Time Variation.	78

<u>Figure</u>		<u>Page</u>
3.7	DC to AC Power Converter	83
3.8	Voltage Equalization Circuit	86
3.9	Current Equalization Circuit	86
3.10	Current Sharing Transformer.	87
3.11	Inverter Processes	89
3.12	Equivalent Circuit of Figure 3.11.	90
3.13	Inverter Generalized Circuit	94
3.14	Inverter Voltage Waveshape	95
4.1	Computer Program Flow Diagram - DC to AC Power Converter	106
5.1	Diagram of the Flow Section of the Channel	108
5.2	Voltage-current Characteristic of the MHD Generator	111
5.3	Variation of Temperature with Channel Distance for Three Different Load Current Conditions .	112
5.4	Variation of the Gas Velocity with Channel Distance for Three Different Load Current Conditions	113
5.5	Variation of Pressure, Current Density and Hall Parameter with Channel Distance. Load Current = 5000 amperes.	114
5.6	Variation of Pressure, Current Density and Hall Parameter with Channel Distance. Load Current = 10000 amperes	115
5.7	Steady State Electrical Parameters	118

<u>Figure</u>	<u>Page</u>
5.8 Steady State Electrical Parameters	119
5.9 Steady State Gas Dynamic Parameters.	120
5.10 _a Response of the MHD Generator to a + 52 % Step Change in the Load Resistance	122
5.10 _b Response of the MHD Generator to a + 52 % Step Change in the Load Resistance (Expanded Time Scale of Figure 5.10a)	123
5.11 Gasdynamic Parameters at $I_L = 16000$ amperes and at $I_L = 12480$ amperes.	124
5.12 Variation of E_x and J_y with Channel Distance and time	125
5.13 Variation of the Gas Velocity and the Gas Temperature with the Channel Distance in the Diffuser and Time.	128
5.14 Open-loop Response of the Steam Plant to a + 5 % Step Change in the Combustion Gas Temperature at the Inlet of the Radiant Section.	133
5.15 Response of the Steam Plant to a (1) + 5 % Step Change in the Combustion Gas Temperature and to a (2) + 5 % Step Change in the Gas Mass Flow Rate	134
5.16 Open-loop Response of the Steam Plant to a + 5 Step Change in the (1) Combustion Gas Temperature and (2) the Combustion Gas Mass Mass Flow Rate	135
5.17 Response of the Steam Plant to a + 5 % Step Change in the Combustion Gas Temperature, the Steam Mass Flow Rate and the Governor Valve Opening	137
5.18 Response of the Steam Plant to a + 5 % Step Change in the Combustion Gas Temperature, the Steam Mass Flow Rate and the Governor Valve Opening.	138

LIST OF SYMBOLS

<u>Symbol</u>	<u>Page first Encountered</u>
a	electrode width, meter 38
	speed of sound, meters/sec 100
A	channel cross-sectional area, sq meter 28
b	electrode pitch, meter 38
b*	electrode pitch to electrode separation ratio 48
B	magnetic induction, webers/sq meter 3
c	speed of light, meters/sec 24
C _f	friction coefficient 65
c _l	friction coefficient 31
C _h	convective heat transfer coefficient 32
C _g	specific heat of the gas, Joules/Kg-°K 66
C _p	specific heat of the gas at constant pressure 33
D	channel hydraulic diameter, meter 28
E	applied electric field, volts/meter 3
E _i	induced electric field, volts/meter 3
f	friction factor 31
F	force per unit volume 3
	friction force per unit volume 29
G	heat transfer per unit volume 29

<u>Symbol</u>		<u>Page first encountered</u>
g	local gravity, meters/sec ²	65
g_i	statistical weight of the ground state of the ion	35
g_o	statistical weight of the ground state of the neutral atom	35
h	specific enthalpy of the gas, Joules/Kg	23
	electrode separation, meters	38
I_L	load current, amperes	43
I^*	dimensionless current	43
J	current density, amperes/ sq meter	3
K	loading parameter	3
k_x		
k_y	coefficients in equations 2.64-2.65	48
k		
L	channel length, meters	5
M	molecular weight of the working fluid, Kg	34
n_e	electron density	35
n_i	ion density	35
n_s	neutral seed atom concentration	35
p	pressure, Newtons/sq meter	5
P	Electrical power, Joules/sec	139
P_i	Electrical power input	5
P_o	Electrical power output	5

<u>Symbol</u>	<u>Page first encountered</u>
P_r Prandtl Number	32
q_c convective heat transfer to the wall, Joules/sec-meter	31
q_g convective heat transfer to the wall	65
q_r radiative heat transfer to the wall	31
q_s heat transfer to the fluid	65
R gas constant	23
recovery factor	32
Re Reynolds number	31
R_m magnetic Reynolds number	26
S Boltzman's constant, Joules/ $^{\circ}K$	34
t time	21
T temperature	32
T_{aw} adiabatic wall temperature	32
T_w wall temperature	32
u gas axial velocity	3
u, u' parameter from Dzung [116] in eq. 2.26-2.28	48
$\bar{v}$ gas velocity	21
v, v' parameter from Dzung [116] in eq. 2.26-2.28	48
V electric potential	46
volume	67
w mass flow rate, Kg/sec	65

<u>Symbol</u>	<u>Page first Encountered</u>
W mass, Kg	68
x, y, z space coordinates	3
α cross-connection parameter	42
β_e Hall parameter, electron	27
β_i Hall parameter, ion	27
δ boundary layer thickness, meter	52
ϵ permittivity, Farads/meter	24
ϵ_g gas emissivity	34
ϵ_w wall emissivity	34
κ thermal conductivity, Joules- ^o K/sec-meter	22
μ permeability, henry/meter	24
η gas viscosity, poise	23
η_e electrical (isentropic) efficiency	43
ω angular frequency, radians/sec	25
ϕ electrode voltage drop parameter	45
Φ electric potential, volts	48
ρ mass density, Kg/cu meter	21
ρ charge density, Coulombs/cu meter	21
σ electrical conductivity, mho/meter	3
τ shear stress	21
Hall angle	49

SymbolPage first
encountered

τ_w wall shear stress

31

Subscripts

d diffuser
 e economizer
 g gas
 h high pressure turbine
 i inlet
 l low pressure turbine
 o outlet
 p radiant section
 r reheater
 s primary superheater, steam
 w wall
 ac air compressor exit
 al low temperature air heater exit (air side)
 do diffuser exit (steam side)
 ei high temperature economizer inlet (steam side)
 eo high temperature economizer exit (steam side)
 em high temperature economizer metal
 ge high temperature economizer exit (gas side)

Subscript

gp radiant section exit (gas side)
 gr reheater exit (gas side)
 gs primary superheater exit (gas side)
 hi high pressure turbine inlet (steam side)
 ho high pressure turbine exit (steam side)
 li low pressure turbine inlet (steam side)
 lo low pressure turbine exit (steam side)
 pm radiant section metal
 rm reheater metal
 si primary superheater inlet (steam side)
 so steady state or design value (steam side)
 sm primary superheater metal
 gah high temperature air heater exit (gas side)
 gal low temperature air heater exit (gas side)
 alm low temperature air heater metal

ABSTRACT

A mathematical model of a magnetohydrodynamic-steam electrical power generation system was developed, incorporating the results of experimental and theoretical investigations conducted by various researchers, applicable to large power generation. A real gas at subsonic condition (typical for large power generation) is used in the flow calculation. A quasi one-dimensional MHD generator model was used to explore the advantages of the model in the design of the control mechanisms for the system. The numerical techniques used in the simulation are described and the results of the simulation are presented. The behavior of the MHD generator in response to load changes are discussed. The dynamic model of the direct-current (DC) to alternating-current (AC) power converter is presented and the design parameters compatible with the MHD generator operation are discussed. The dynamic model of the steam plant and air heaters was developed and the results of the simulation of the model in response to changes in input conditions are shown. The design parameters of the steam plant for a 2000 megawatt (thermal input) MHD system are given.

Chapter I

INTRODUCTION

1.0 System Simulation

The goal of developing a large Magnetohydrodynamics (MHD) Electrical Power Generation System has created a need for the study of the system performance under normal and abnormal operating conditions. Simulation provides one means for investigating the behavior of the system and aids in predicting system performance under specified operating conditions. The system may be represented by a scaled, pilot or analytical (mathematical) model. After considering the advantages and the disadvantages of each model, such as flexibility, cost, time and responsiveness, the mathematical model was selected for this study.

The mathematical model consists of partial and ordinary differential equations which accurately define the system. Since the simulation is implemented by the use of a digital computer, numerical methods are utilized in solving the defining equations.

1.1 Open-cycle MHD-Steam Electrical Power Generation

Magnetohydrodynamics or more correctly magnetofluidmechanics is the science dealing with the interaction between an electrically

conducting fluid and a magnetic field. For a compressible fluid, part of the kinetic and thermal energy is converted directly into electricity. A simple MHD generator is shown in Figure 1.1. The generator consists of a channel of rectangular cross-section with one pair of electrically conducting electrodes and one pair of electrically insulating walls. The load is connected between the electrodes, and the magnetic field is perpendicular to the insulating wall.

In an open-cycle MHD generator, the fluid consists of the combustion gases from the burning of fossil fuels, seeded with an alkali metal to increase the electrical conductivity. A combustion temperature of 2500° Kelvin to 2800° Kelvin is necessary to provide enough electrical conductivity and thermal energy for an efficient energy conversion.

The fluid flow is accelerated through a nozzle and enters the MHD duct. The magnetic field acts to decelerate the charge carriers in the fluid stream. In the absence of energy transfer from the other particles, the electrons spin in a circular orbit about the transverse axis in the magnetic field. The fluid stream forces the electrons through the applied magnetic field. The net effect is a reduction in the kinetic energy of the fluid stream and an equivalent direct current electrical power is induced in the external load circuit. The coordinate system used to describe the phenomena taking

place in the generator is shown in Figure 1.2.

If a steady state condition is assumed, and one-dimensional flow approximations are used, the voltage induced by the interaction of the velocity field with the magnetic field is

$$E_i = uB \quad (1.1)$$

and the current density is

$$J = \sigma(uB - E) \quad (1.2)$$

where the notation is defined in the symbol reference list.

Following Rosa [1], a loading factor (K) may be defined as

$$K = E/uB \quad (1.3)$$

so that

$$J = \sigma(1 - K)uB \quad (1.4)$$

The power delivered to the load per unit volume is

$$P_o = JE = \sigma K(1 - K)u^2 B^2$$

The current interacts with the magnetic field to induce a force (F) opposed to the fluid motion. The force on a unit cube of the fluid is

$$F = JB = \sigma(1 - K)uB^2 \quad (1.5)$$

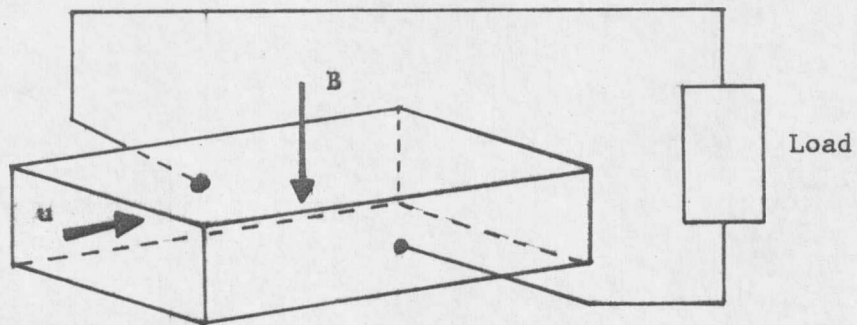


Figure 1.1. A Simple MHD Generator

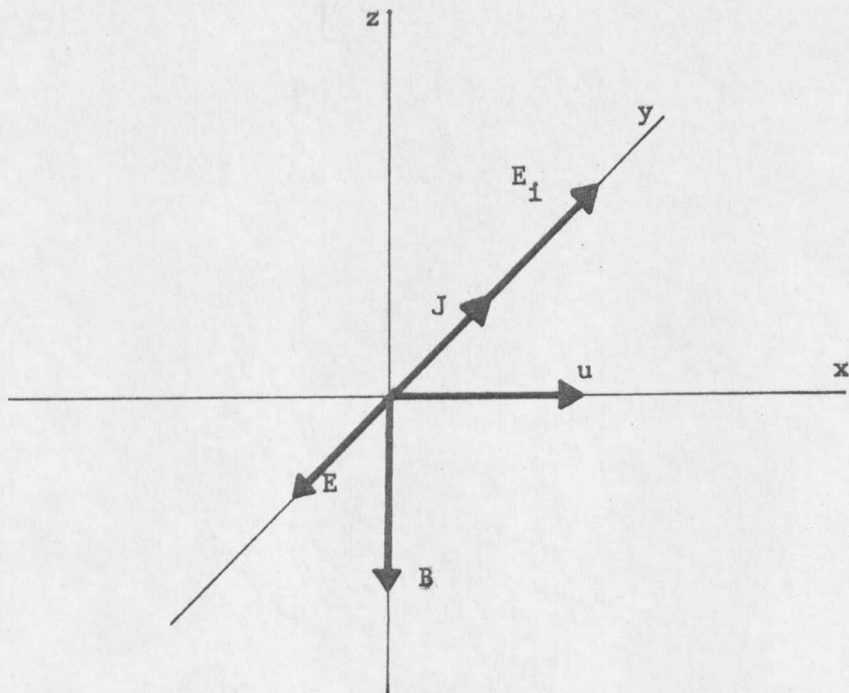


Figure 1.2. Coordinate System.

In order for the gas to move through the generator, a pressure difference between the entrance and the exit must exist and is given by (neglecting friction effects)

$$p \approx FL = \sigma(1 - K)uB^2L \quad (1.6)$$

where L is the generator length.

The work done by the gas in pushing itself through the magnetic field is

$$P_i = Fu = \sigma(1 - K)u^2B^2L \quad (1.7)$$

Thus the electrical efficiency is

$$\eta_e = P_o/P_i = K \quad (1.8)$$

Since the energy dissipation in this device occurs within the working fluid, it is not lost energy, and can still be recovered in a bottoming plant.

In the MHD generator, the electrical conductivity of the fluid decreases rapidly with the decreasing gas temperature, thus it becomes uneconomical to extract electrical power directly from the fluid by the MHD process below a fluid temperature of 2100°K . The thermal energy of the fluid leaving the MHD generator can be used for heating the air needed in the combustion process (to attain high combustion

temperature) and to produce steam for a steam turbine to generate more power (see Figure 1.3). The MHD generator thus operates as a topping plant for a steam-electric power plant making it possible to obtain higher thermal efficiency than existing conventional thermal electric power plants.

1.2 Historical Review

Magnetohydrodynamics Electrical Power Generation

Magnetohydrodynamics electrical power generation is based on the Faraday effect in which a voltage is induced in a circuit when a magnetic field linking the circuit is changed. This effect was first observed by Michael Faraday [2] in 1831, when he experimented with mercury flowing through a magnetic field. Industrial experiments with gaseous MHD were conducted by Karlovitz [3] in the period 1938 to 1944. The generator failed to operate because of the low conductivity of the working gas.

Extensive work started again in the late fifty's and early sixty's in the United States [4-8]. The first combustion experiment was performed at Westinghouse Electric Corp. [8] and followed by similar experiment [9]. The cycle analysis of the MHD electrical power generation system [10-17] showed that if a MHD generator is operated above 2500°K , and used as a topper for a steam plant, high thermal efficiencies can be obtained. Considerable work had been done

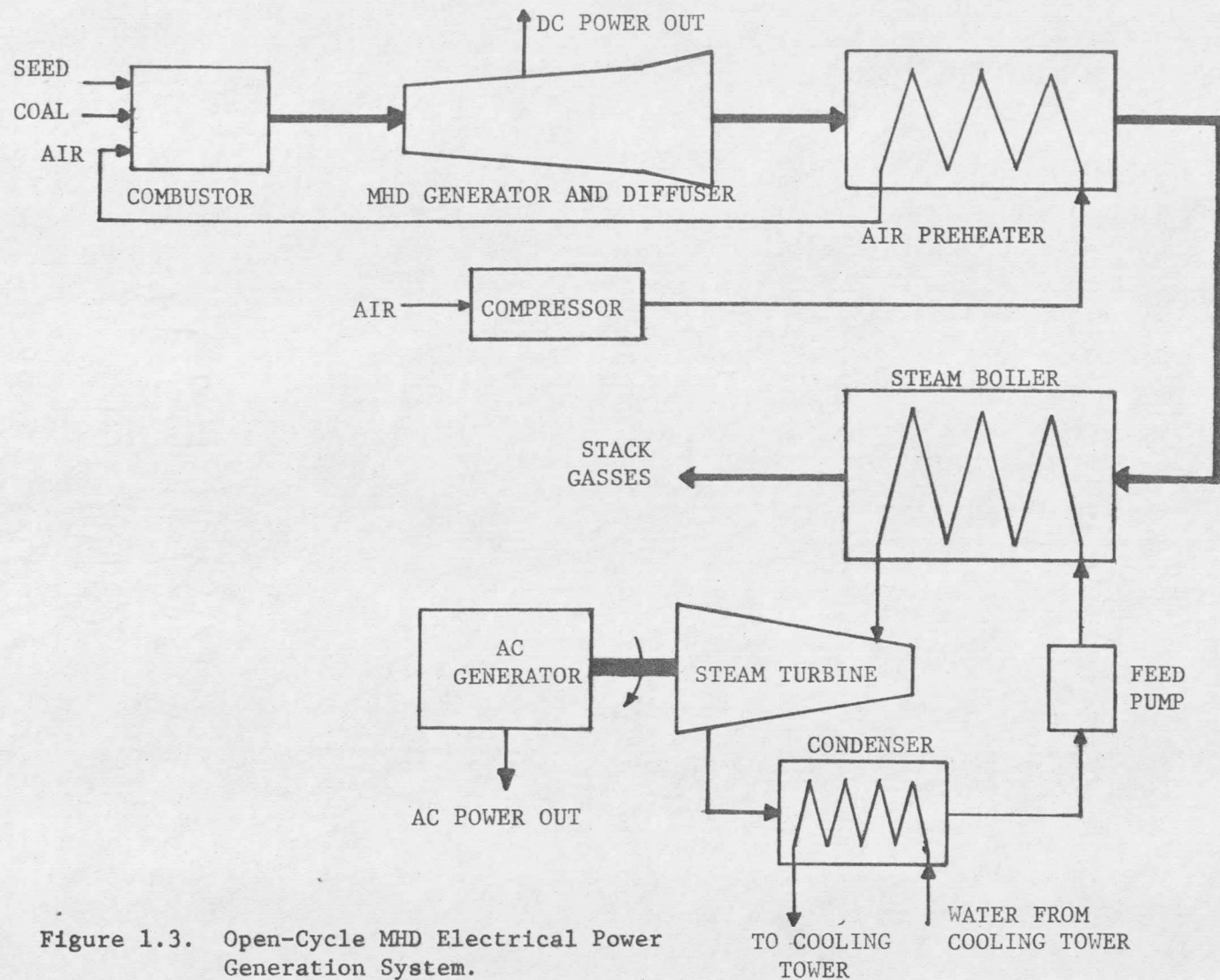


Figure 1.3. Open-Cycle MHD Electrical Power Generation System.

also on the environmental aspects of MHD [18-20]. The high temperature MHD energy conversion process, with the higher attainable thermal efficiency it makes possible, also provides opportunity for the reduction and control of air and thermal pollution from the power plant.

One-dimensional Steady State Flow

Extensive research has been conducted in the United States [21-30], and other countries [31-36] to better understand the MHD process and to solve the technological problems associated with the phenomena. The development of a reliable method of calculating the flow of a fluid in a MHD channel is one of the most important problems, the solution of which may aid in the development of MHD generators. Considerable research work has been devoted to this problem. The complexity of three-dimensional models [37-38], led early researchers to consider one-dimensional approximations.

The analysis of the MHD generator steady state response with an ideal gas as a working fluid was performed by several researchers [7, 39,40]. Some of the studies devoted to the comparison of the results of the one-dimensional theory with experiments [22-25] show that this theory may be used for the calculation of the flow in the channel of large MHD generators if the effects of the boundary layer and the end region nonuniformities are neglected. The one-dimensional theory is adequate for the calculation of the power output, electromagnetic

pressure gradient and load current. In references [41-42], the heat transfer and friction effects are treated as a fixed percentage of the MHD generator output, while in [43-45], the heat transfer and friction effects are calculated as functions of the gas parameters (temperature, pressure, velocity, mass density, etc.).

Numerical calculation of the electrical parameters in a Faraday-type MHD generator with two-dimensional gas flow was performed by Celinski [46], in which the effects of the viscous and thermal boundary layers on the power output of the MHD generator was considered.

One-dimensional Time-dependent Flow

Most of the analytical work done relating to the development of the MHD method of energy conversion, considered only the steady-state problem of the flow. Earlier work on time-dependent MHD channel flow done by Yen and Chang [47], considered a time dependent pressure gradient in the channel. The convolution integral and superposition principle was used to obtain the solution for any arbitrary time-dependent pressure gradient. Lundgren, et al. [48], did an analysis of the transient MHD duct flow of an electrically conducting viscous incompressible fluid. The results showed that the response of the fluid was similar to the response of a mass-spring system with a damper. The computational problems in MHD (time-dependent and time-independent flow) were also studied by Killeen [49]. A review of a number

of computational techniques useful in solving the equations of MHD was presented. A review of existing literature and experimental investigation of non-steady state flow of a viscous incompressible, electrically conducting fluid in MHD channels can be found in reference [50].

In open-cycle MHD electrical power generation systems, the working fluid is a real, viscous, compressible multi-component gas. There are several research studies devoted to this problem [51-56]. In reference [51], the problem of one-dimensional unsteady-state flow of an electrically conducting fluid in the channel of the MHD generator and the numerical solution are discussed. The results of the numerical solution of the problem of a sudden change in the load parameters are given. A small MHD channel (.961 m x .5 m x .144 m) was considered. In reference [52], the problem of the input shock wave into the channel with continuous electrodes and constant cross-section is established and numerically solved. One-dimensional non-steady state motion of an inviscid non-heat conducting gas behind the front of a shock wave are considered. End effects and current leakage in the channel are neglected. In reference [53], the results of a numerical solution of the problem of unsteady-state flow with ideal gas and external load circuitry are given. The external circuit consists of a resistance and inductance. In a later paper [54], the flow of a real gas in a channel of variable cross-sectional area was considered. The channel includes

the nozzle, MHD generator and diffuser. The gas is assumed to be flowing from a large reservoir such that the boundary conditions may be considered fixed. An artificial viscosity [57] was introduced into the equation describing the flow, making it possible to calculate the separation of flow (discontinuity) in the channel. In this reference, the results of the numerical solution for a fairly large MHD channel are presented. The channel is 10 meters long (nozzle - 2 meters, MHD generator - 5 meters and diffuser - 3 meters). Rosciszewski and Yeh [56] performed a numerical calculation of a non-steady, non-equilibrium flow in a divergent linear MHD generator. The purpose of the research work was to determine the time necessary for the establishment of a steady state condition at different generator loads and gas input parameters. The two-step Lax-Wendroff finite difference method [57] was used to solve the MHD equations. Oliver [155] conducted a study of the transient response of a MHD generator to changes in the load condition. Two types of generators were considered, a constant velocity supersonic single load diagonal wall generator and a constant velocity subsonic multiple load diagonal wall generator. The working fluid was assumed to be an ideal gas. The two step-Lax Wendroff method was also used to implement the computer calculation. The response of the generator to a finite amplitude magnetoacoustic disturbance was obtained.

Direct-current to Alternating-current Power Conversion

The increasing use of high-voltage direct-current (HVDC) power transmission has created the need for dynamic models to analyze the performance of alternating-current to direct-current converters. A digital computer simulation provides a versatile and accurate alternative to a scaled physical model. The most suitable configuration for HVDC operation is the basic bridge or Graetz [57] circuit. (See Figure 1.4). The behavior of this three-phase bridge converter under steady-state normal operation is fully documented [57-58]. The conversion of power at each end of the HVDC link may be carried out by mercury arc converters or by thyristors.

In 1966, Hingorani, et al developed a new digital computer technique for the study of HVDC systems. The technique is called the Central Process Method based on the fact that the operation of a HVDC converter consists of similar consecutive processes. Each bridge converter is represented by a set of differential and boolean equations. This technique may be used to obtain the waveform of transient and steady state voltages and currents anywhere in the HVDC system. In a later paper [60], the simulation of abnormal conditions (faults) in a HVDC system was presented.

Hingorani and Hay [61-63], developed a more flexible and accurate technique in digital simulation of HVDC systems. The digital computer

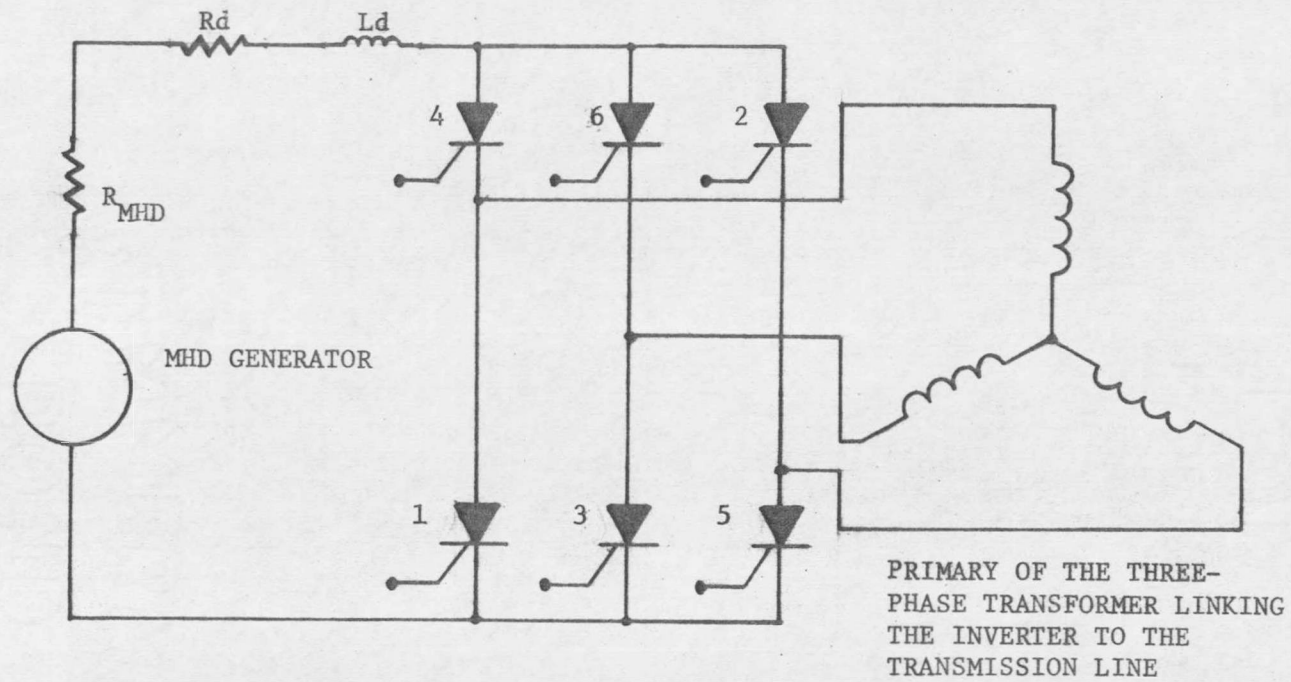


Figure 1.4. A Three-Phase Bridge Inverter Circuit

program may represent different circuit configurations, modes of control and protective devices. Hingorani and Mountford [64], also developed a method by which trunk lines may be simulated for the purpose of load flow analysis by digital computer. The power system may contain one or more DC transmission links.

Steam Plant Dynamic Modelling and Simulation

The development of the mathematical model of a steam plant characterizing the dynamic behavior of each component is necessary for the dynamic control of the plant. A number of mathematical models have been made to describe the dynamics of steam plant components [65-73]. In 1958, Chien, et al. [65], provided the first comprehensive dynamic analysis of a steam boiler. The model was a simple representation of an oil-fired, single furnace, natural circulation marine boiler. A formulation of the mathematical model of a 200 MW electric power generating station (Cromby Unit No. 2 of Philadelphia Electric Co.) was published by Daniels, et al. [66] in 1960. The system includes a pulverized coal-fired, twin furnace, controlled circulation reheat boiler. In 1964, Nicholson [67], developed optimal and suboptimal digital controllers for the control of an oil-fired boiler model specified by discrete state variables and transition function. The dynamic model used was similar to that of Chien, and the steady state test data was obtained from a boiler installed at an industrial power sta-

tion at Merseyside, Gt. Britain. Thompson [68], published in 1965, a refined version of the Daniels model. A linearized mathematical model which describes the dynamics of the drum, downcomer, waterwall, economizer, superheater and the combustion portion of a large utility power plant was developed. The state variable technique was used in implementing the model. A comparison with the dynamic response of the Cromby Unit No. 2 was made.

In 1968, Anderson, et al [69], developed a mathematical model for a 200 MW coal-fired, drum type, natural circulation boiler with reheat. The differential equations characterizing the behavior of each component were linearized and manipulated into state space form for solution in a digital computer. Speedy [70], studied the least squares procedure in estimating the parameters of a steam generating plant model. The results showed that the least squares method provides a simple procedure for obtaining mathematical models of steam generating plant with root-mean-square (rms) prediction accuracy better than 5 %.

In 1971, McDonald, et al [71-72], developed a non-linear mathematical model of a drum type, twin furnace, reheat boiler-turbine generator system which is suitable for control system analysis and design. All model parameters were obtained from design data and unit acceptance test data of the system. The system under study was the

coal-fired, 200 MW Cromby Unit No. 2. An extensive comparison of the model simulation with the field data was made. Excellent agreement was found in both closed loop steady state operation and open-loop transient response. McDonald and Kwatny [73], extended their study to the development of a methodology for the design and analysis of multi-variable process controls and its application to the control of conventional, drum-type, fossil-fired, single reheat steam power plants based on optimal linear control theory.

The bottoming plant of the MHD system is different from a conventional steam plant. In the MHD system, the boiler is an unfired waste-heat boiler without the losses associated with the furnace. The air preheater in the conventional plant is used to lower the stack gas temperature, whereas in the MHD system, the air is first compressed, thus increasing its temperature. Feedwater heating is accomplished by stack gas cooling, wall cooling of the combustion chamber, MHD channel and diffuser and also by the regenerative method.

Hoover, et al. [14], proposed a steam bottoming plant for the MHD system operating at 2.4×10^7 Newtons/sq m / 822° Kelvin with single reheat and the steam exhausting at 3.8×10^{-2} m mercury. There are four steam turbines, one high pressure, one intermediate pressure and two low pressure turbines developing an aggregate power approximately equal to one-half of the total MHD system power output. Steady

state performance of the steam plant was evaluated.

Laxton and Steyens [74], provided a first comprehensive design of a proposed steam bottoming plant based on a 2000 MW thermal input to the MHD system. The steam plant operates at a supercritical conditions (2.4×10^7 Newtons/sq m, 822°Kelvin) and single reheat. The steam turbine consists of one high pressure, one intermediate pressure and one low pressure turbine. Only the steady state performance of the steam plant was investigated.

1.3 Statement of the Problem

The problems of the steady state flow of a conducting fluid in the channel of a MHD generator have been extensively investigated. The prospects of the development of large industrial MHD electrical power generation system creates an urgent need for the solution of the problems associated with the time-dependent flow in the channel and the analyses of the dynamic response of the system components.

The dynamic response characteristics of a large MHD electrical power generation system may be obtained by using a scaled physical model or by the computer simulation of the mathematical model. It is expensive to build and operate a scaled model and it is difficult for the scaled model to accurately represent the dynamic response of a large system. Hence, a digital computer simulation of the mathematical model is usually chosen because of its low cost and great flexibility.

Alivadze, et al. [53-55] have investigated the unsteady state flow in the channel of a MHD generator. A real channel of variable cross-section was considered, together with a nozzle and diffuser in which a real gas is flowing. An artificial viscosity [75] term was introduced into the flow equations, which made it possible to calculate separation of flow (discontinuity) in the channel. The dynamic response of the generator was obtained under different load conditions. A Faraday segmented electrodes generator was considered.

Since a large MHD electrical power generation system includes not only the MHD generator and diffuser but also the steam bottoming plant, the air heater, the combustor and the DC to AC converter, it is important that the behavior of these components should be carefully analyzed. The dynamic control of the system can then be based on the results of this study.

In this study, the MHD system components considered are the MHD generator, the diffuser, the steam bottoming plant and the air heater. The dynamic models of these components were developed and the transient response to changes in operating conditions were obtained. In the MHD generator, the transient response to changes in load condition is investigated. The response of the steam plant to the variation of the gas parameters (such as the mass flow rate and the gas temperature) is carefully analyzed. The transient behavior of the DC to AC conver-

ter is also investigated.

The outline of the thesis is as follows:

Chapter II. The dynamic model of a single-load segmented MHD generator is developed. Basic assumptions leading to the development of the quasi one-dimensional model are presented. The limitations of the model is also discussed.

Chapter III. The dynamic model of the steam bottoming plant and air heater is developed based on the governing laws of fluid dynamics, empirical heat transfer relationships and the equation of state. The DC to AC converter is also represented by a mathematical model.

Chapter IV. The implementation of the digital computer simulation of the mathematical model of the MHD electrical power generation system components is presented. The numerical methods used are also discussed.

Chapter V. In this chapter, the results of the digital computer simulation with the appropriate figures to illustrate the dynamic response of the system are presented.

Chapter VI summarizes the results and the conclusions obtained from the simulation. The significance of the results are discussed and the suggestions for future research are given.

Chapter II

FORMULATION OF THE MATHEMATICAL MODEL OF A MAGNETOHYDRODYNAMIC GENERATOR

2.0 Introduction

In this chapter, the mathematical model of a magnetohydrodynamic (MHD) generator is formulated. The three-dimensional MHD equations are first described and the MHD approximations applicable to MHD electrical power generation are presented. The quasi one-dimensional MHD flow applicable to a large electrical power generation system is developed. The limitations of the model are also discussed.

2.1 Magnetohydrodynamics Equations

MHD electrical power generation involves the flow of a viscous, compressible, electrically conducting fluid through a channel in the presence of a transverse magnetic field. As in fluid dynamics, the fluid may be considered as a continuous medium. The MHD equations consist of the Navier-Stokes equations of fluid dynamics, the energy equation, Maxwell's equations, the equation of state of the working fluid and Ohm's law. The derivation of these equations can be found in references [76-78]. Neglecting the effect of magnetization and polarization [77], the MHD equations are (per unit volume):

Fluid Dynamics Equations

Mass Conservation or Continuity Equation. This equation expresses the principle of conservation of mass, i.e. the time rate of change of mass in an arbitrary volume is equal to the total mass flow out of that volume.

$$\frac{\partial \rho}{\partial t} + \nabla \cdot (\rho \underline{v}) = 0 \quad (2.1)$$

The Equation of Motion or the Momentum Equation. The body forces acting on the fluid are the Lorentz force ($\underline{J} \times \underline{B}$), the electrostatic or coulombic force ($\rho \underline{E}$), the inertial and pressure forces (∇p) and the viscous force ($\nabla \cdot \underline{\tau}$) [77].

$$\rho \frac{D\underline{v}}{Dt} = -\nabla p + \nabla \cdot \underline{\tau} + \rho \underline{E} + \underline{J} \times \underline{B} \quad (2.2)$$

where $\frac{D}{Dt} = \frac{\partial}{\partial t} + \underline{v} \cdot \nabla$, the convective derivative [77-78].

The Conservation of Energy Equation. The energy equation consists of the internal energy equation and the kinetic energy equation (the product of the momentum equation and the velocity). The kinetic energy equation is

$$\begin{aligned} \rho \frac{D(v^2/2)}{Dt} = & - [(\underline{v} \cdot \nabla) p + \nabla \cdot (\underline{v} \cdot \underline{\tau}) + \underline{v} \cdot \underline{J} \times \underline{B} \\ & + \underline{v} \cdot \rho \underline{E}] \end{aligned} \quad (2.3)$$

and the equation for the internal energy of the working fluid is

$$\rho \frac{DU}{Dt} = - \{ p(\nabla \cdot \underline{v}) + \nabla \cdot (\kappa \nabla T) + J^2/\sigma \} \quad (2.4)$$

where U is the internal energy of the working fluid per unit mass, $p(\nabla \cdot \underline{v})$ is the work done by the pressure, $\nabla \cdot (\kappa \nabla T)$ is the rate of thermal energy lost from the fluid, J^2/σ is the ohmic heating, and $\nabla \cdot (\underline{v} \cdot \underline{T})$ is the dissipation function [77]. The change in the electrical stored energy is negligible because the permittivity of the working fluid is approximately equal to that of free space.

The total energy equation is

$$\begin{aligned} \rho \frac{D}{Dt} (U + v^2/2) = & - \{ \nabla \cdot (\underline{v} p) + \nabla \cdot (\kappa \nabla T) + \underline{v} \cdot \rho_e \underline{E} \\ & + J^2/\sigma + \nabla \cdot (\underline{v} \cdot \underline{T}) \\ & + \underline{v} \cdot \underline{J} \times \underline{B} \} \end{aligned} \quad (2.5)$$

where $(\underline{v} \cdot \nabla) p + p(\nabla \cdot \underline{v}) = \nabla \cdot (\underline{v} p)$ is the rate of mechanical work done by the gas in working against the pressure forces on the surface of the volume. But

$$\underline{v} \cdot \underline{J} \times \underline{B} + J^2/\sigma + \underline{v} \cdot \rho_e \underline{E} = \underline{J} \cdot \underline{E} \quad (2.6)$$

Thus

$$\begin{aligned} \rho \frac{D}{Dt} (U + v^2/2) = & - \{ \nabla \cdot (\underline{v} p) + \nabla \cdot (\kappa \nabla T) + \underline{J} \cdot \underline{E} \\ & + \nabla \cdot (\underline{v} \cdot \underline{T}) \} \end{aligned} \quad (2.7)$$

The specific internal energy is

$$U = h - p/\rho \quad (2.8)$$

and

$$\begin{aligned} \rho \frac{D}{Dt} (p/\rho) &= \rho \frac{\partial}{\partial t} (p/\rho) + \rho \underline{v} \cdot \nabla (p/\rho) \\ &= \frac{\partial p}{\partial t} + \nabla \cdot (\underline{v} p) \end{aligned} \quad (2.9)$$

Hence the energy equation may be written as

$$\begin{aligned} \rho \frac{D}{Dt} (h + v^2/2) &= \frac{\partial p}{\partial t} - \{ \nabla \cdot (\kappa \nabla T) + \underline{J} \cdot \underline{E} \\ &\quad + \nabla \cdot (\underline{v} \cdot \underline{\tau}) \} \end{aligned} \quad (2.10)$$

The equation of State (for an ideal gas) may be written as

$$p = \rho RT$$

The thermodynamic, electrical and transport properties are:

$$\text{The specific enthalpy is } h = h(p, T) \quad (2.11)$$

$$\text{The thermal conductivity is } \kappa = \kappa(p, T) \quad (2.12)$$

$$\text{The viscosity is } \eta = \eta(p, T) \quad (2.13)$$

$$\text{The electrical conductivity is } \sigma = \sigma(p, T) \quad (2.14)$$

Maxwell's equations are:

$$\nabla \cdot \underline{E} = \rho_e / \epsilon \quad (2.15)$$

$$\nabla \cdot \underline{B} = 0 \quad (2.16)$$

$$\nabla \times \underline{E} = - \frac{\partial \underline{B}}{\partial t} \quad (2.17)$$

$$\nabla \times \underline{B} = \mu (\underline{J} + \epsilon \frac{\partial \underline{E}}{\partial t}) \quad (2.18)$$

2.2 MHD Approximations

The process involved in a MHD flow is described accurately by the three-dimensional fluid dynamic equations, Ohm's law and Maxwell's equations. In MHD electrical power generation, a number of approximations can be made which will be valid over a wide range of operating conditions. These are:

(1) The fluid flow involves velocities much smaller than the velocity of light, thus relativistic effects are neglected. In large MHD generators the fluid velocity is typically in the order of 10^3 m/sec. The relativistic factor $(\sqrt{1 - v^2/c^2})$ may then be considered equal to unity.

(2) The displacement current in the electrically conducting medium is very small compared to the conduction current over a wide range of frequency. The ratio of the amplitude of the displacement

current ($\epsilon \frac{\partial E}{\partial t}$) to the amplitude of the conduction current is approximately

$$\epsilon \left(\frac{\partial E}{\partial t} \right) / \sigma E_{\max} \approx \frac{\epsilon \omega}{\sigma}$$

In open-cycle MHD energy conversion where the working fluid is the gaseous products of the combustion of fossil fuel and air, the typical parameters are $\sigma \approx 2 - 10$ mho/m and $\epsilon \approx 10^{-12}$. This gives a ratio of 10^{-13} which shows that even at very high frequencies the displacement current is negligible.

(3) The magnitude of the excess charge density (ρ_e) may be obtained from the equation $\epsilon \nabla \cdot \underline{E} = \rho_e$ (Gauss's law). Since $\underline{E}$ varies linearly over some small region, the excess charge density is approximately equal to E/L , where L is the characteristic length of the electric field. The ratio of the magnitude of the current flow due to the excess charge to the amplitude of the conduction current is approximately

$$\rho_e u / \sigma E = \epsilon E u / L \sigma E = \epsilon u / L \sigma$$

Typical values of u and L are 10^3 m/sec and 1.5 m respectively. This gives a ratio of 10^{-10} , which means that the excess charge current can be safely neglected.

(4) The electrostatic body force in the working fluid is equal to $\rho_e \underline{E}$. The ratio of this force to the magnetic force (Lorentz) is

$$\rho_e \underline{E} / \underline{J} \times \underline{B} = \epsilon E / [LB\sigma(E + uB)]$$

The applied electric field (E) is the same order of magnitude as uB , thus

$$\underline{J} \times \underline{B} \approx \sigma(uB^2)$$

and the ratio is then equal to

$$\epsilon E^2 / \sigma L u B^2 = \epsilon u / \sigma L$$

which is in the order of 10^{-10} .

(5) The induced magnetic field is equal to $\mu \sigma L u B$. Dividing the induced magnetic field by the applied magnetic field B , gives the magnetic Reynolds number, $R_m = \mu \sigma L u$. The typical value for open-cycle MHD electrical power generation is approximately 10^{-2} , which means that the induced magnetic field can be safely neglected.

Using the above approximations, the MHD equations may then be written as

Continuity equation

$$\frac{\partial \rho}{\partial t} + \nabla \cdot (\rho \underline{v}) = 0 \quad (2.19)$$

Momentum equation

$$\rho \frac{D\mathbf{v}}{Dt} = -\nabla p + \nabla \cdot \underline{\underline{\tau}} + \underline{\underline{J}} \times \underline{\underline{B}} \quad (2.20)$$

Energy equation

$$\begin{aligned} \rho \frac{D}{Dt} (h + \mathbf{v}^2/2) + \frac{\partial p}{\partial t} + \nabla \cdot (\kappa \nabla T) + \underline{\underline{J}} \cdot \underline{\underline{E}} \\ + \nabla \cdot (\underline{\underline{v}} \cdot \underline{\underline{\tau}}) = 0 \end{aligned} \quad (2.21)$$

Ohm's law

$$\underline{\underline{J}} = \sigma (\underline{\underline{E}} + \underline{\underline{v}} \times \underline{\underline{B}}) - \beta_e (\underline{\underline{J}} \times \underline{\underline{B}})/B + \beta_e \beta_i [(\underline{\underline{J}} \times \underline{\underline{B}}) \times \underline{\underline{B}}]/B^2 \quad (2.22)$$

Maxwell's equations

$$\nabla \cdot \underline{\underline{J}} = 0 \quad (2.23)$$

$$\nabla \cdot \underline{\underline{B}} = 0 \quad (2.24)$$

$$\nabla \times \underline{\underline{H}} = \underline{\underline{J}} \quad (2.25)$$

$$\nabla \times \underline{\underline{E}} = - \frac{\partial \underline{\underline{B}}}{\partial t} \quad (2.26)$$

2.3 Quasi one-dimensional MHD equations

Dynamic control of a MHD system requires a simple but accurate mathematical model, thus avoiding a complex control implementation problem. Some approximations must be made to reduce the three-dimensional model to a one-dimensional model. However, this does not imply that the process in the MHD channel is one-dimensional.

The approximations are:

(1) The variation of the cross-sectional area of the channel is such that the only significant component of the velocity is in the axial direction. This implies that $A \frac{dA}{dx} \ll 1$ [80].

(2) All non-uniformities in the end region (inlet and exit) of the MHD channel can be neglected. These non-uniformities are associated with eddy currents in the end regions. These eddy currents tend to reduce the power output of the MHD generator. However, the losses may be reduced by increasing the generator aspect ratio (L/D). Methods that may be used in reducing these losses are discussed in references [81-83].

(3) The magnetic field is constant and parallel to the z -axis and the electrostatic field is only in the x and y direction. The magnetic field is uniform over the cross-section of the duct.

(4) The induced magnetic field is negligible. This implies that the magnetic Reynolds number (a measure of the ease with which the fluid flows through a magnetic field) is smaller than unity. For open-cycle MHD, the magnetic Reynolds number is very much less than unity. Thus, the perturbation of the magnetic field is very small.

(5) The fluid flow parameters (velocity, temperature, pressure and mass density) are assumed to be uniform over the cross-section. This implies that the flow parameters may be represented by its average

value over the cross-section of the channel.

The quasi one-dimensional equations may then be written:

Continuity

$$\rho \frac{\partial \rho}{\partial t} + \frac{1}{A} \frac{\partial}{\partial x} (\rho u A) = 0 \quad (2.27)$$

Momentum

$$\rho \frac{\partial u}{\partial t} + \rho u \frac{\partial u}{\partial x} = - \frac{\partial p}{\partial x} + (JB)_x - F \quad (2.28)$$

Energy

$$\rho \frac{\partial h}{\partial t} + \rho u \frac{\partial h}{\partial x} + \rho u \frac{\partial h}{\partial x} + \rho u^2 \frac{\partial u}{\partial x} = \frac{\partial p}{\partial t} + JE - G \quad (2.29)$$

Ohm's law

$$J_x = \frac{\sigma}{(1 + \beta_i \beta_e) + \beta_e^2} [(1 + \beta_i \beta_e) E_x - \beta_e (E_y - uB)] \quad (2.30)$$

$$J_y = \frac{\sigma}{(1 + \beta_i \beta_e) + \beta_e^2} [(1 + \beta_i \beta_e) (E_y - uB) + \beta_e E_x] \quad (2.31)$$

where $F = \frac{\partial \tau}{\partial x}$ and $G = \frac{\partial}{\partial x} (\kappa \frac{\partial T}{\partial x})$

The MHD generator may be operated as a constant area, constant velocity, constant Mach number, constant temperature, constant pressure or constant density generator. The constant area generator is very useful for MHD experiments. For subsonic and supersonic inlet conditions, the flow will tend to a Mach number of unity at the exit [84].

The thermal energy is converted into kinetic energy for a subsonic inlet condition and the kinetic energy will be converted to thermal energy for the supersonic inlet conditions. These converted energies cannot be recovered in the duct, thus the conversion efficiency is reduced. In the constant velocity generator [85], the gas temperature decreases so that the Mach number at the exit is greater than at the inlet resulting in an increasing diffuser losses. In a constant temperature generator, the electrical energy is obtained from the kinetic energy only [86]. In the constant pressure or density MHD generator, only a part of the flow kinetic energy is converted to electrical energy, the rest being converted to thermal energy. The constant Mach number generator (for a perfect gas in the absence of heat transfer), when optimized provides the maximum electrical power output with minimum duct size [87]. In a generator utilizing real, viscous, compressible gas as a working fluid, the optimum (minimum duct size for a maximum power output and fixed pressure ratio) is close to a constant Mach number generator [45].

2.4 Heat Transfer and Friction Effects

The quasi one-dimensional model ignores the effects of the viscous and thermal boundary layers. Due to MHD forces, electrical dissipation near the electrode wall, the deposition of the condensed seed and slag on the wall, the existing boundary layer theory developed for

rocket engine nozzles [88] may not be applicable to MHD generators. In the development of the heat transfer and friction effects equations, the boundary layer is ignored. The boundary layer effects are discussed in section 2.7.

The heat transfer (G) and friction (F) terms in the quasi one-dimensional equations may be expressed in terms of the heat flux and shear stress at the duct wall. It is assumed that the heat transfer and friction effects are the same on the electrode and insulator walls. The momentum and energy balance equations for an element of the duct length are [89]

$$F = 4 \tau_w / D \quad (2.32)$$

$$G = 4(q_c + q_r) / D \quad (2.33)$$

The shear stress (τ_w) is calculated from the friction factor

$$f = 2 \tau_w / \rho u^2 \quad (2.34)$$

The flow in a segmented MHD duct may be considered similar to a fully developed flow in a long rough tube. The flow in a duct is controlled by the Reynolds number ($Re = \rho u D / \eta$). For a large MHD generator with thermal input greater than 2 megawatts, the range of the Reynolds number is 10^5 to 10^7 (turbulent flow). For this range, the friction factor is approximately [90] , $f = c_1 Re^{-.2}$

The effect of the MHD forces on the value of the friction factor for large MHD generators has not been fully investigated. Various authors [90,91] reported values of friction coefficient (c_1) for small MHD generators equal to three times greater than in the case of a smooth wall. In an investigation conducted by Zankl [92], using a small channel (4 cm x 5 cm inlet, 12 cm x 5 cm exit, 1.6 m long), the larger friction coefficient led to an increase in the temperature and pressure of the combustion gas, thus resulting in the increase of electrical conductivity. However for large generators, the friction coefficient may not be as large as that exhibited by small experimental generators.

The friction term (F) may then be written

$$F = 2 c_1 \rho u^2 Re^{-.2} \quad (2.35)$$

The convective heat flux (q_c) is obtained from the convective heat transfer coefficient (C_h)

$$C_h = q_c / (T_{aw} - T_w) \quad (2.36)$$

where $T_{aw} = T + R u^2 / 2C_p$

$$R = \frac{1}{3} Pr$$

$$Pr = C_p \eta / \kappa$$

In a fully developed pipe flow [89]

$$C_h = c_2 Re^{.8} Pr^{.4} \kappa/D \quad (2.37)$$

where c_2 is a constant which depends on the wall roughness, obtainable from experimental data [93-94]. The value of c_2 is also influenced by the MHD effects, seed and slag deposition on the walls and the Ohmic dissipation on the electrodes.

The convective heat flux may then be written as

$$q_c = c_2 Re^{.8} Pr^{.4} (\kappa/D) (T - T_w + Ru^2/2) \quad (2.38)$$

Let $\Delta T = T - T_w$ and $\Delta h = h - h_w$

where h and h_w are the specific enthalpy at the temperatures T and T_w respectively. But $\Delta h = \Delta TC_p + \Delta p C_t$, thus for small variation of pressure, $\Delta h = \Delta TC_p$, or $h - h_w = C_p (T - T_w)$. The convective heat flux may then be rewritten as

$$q_c = c_2 Re^{.8} Pr^{.4} (\kappa/DC_p) (h - h_w + Ru^2/2) \quad (2.39)$$

The actual convective heat loss is greatly influenced by the slag and seed deposits on the walls which would lead to an increase in the wall surface temperature of the inner walls. This would result in the reduction of the thermal boundary layer thickness.

The radiative heat flux (q_r) is calculated from the radiation

equation [95]

$$q_r = S (\epsilon_g T^4 - \epsilon_w T_w^4) \quad (2.40)$$

The wall emissivity (ϵ_w) depends on the material and condition of the wall surface and the gas emissivity (ϵ_g) depends on the duct dimension and the working fluid constituents [95]. An approximate formula for (ϵ_g) extrapolated from the curves obtained by McAdams [95] is

$$\epsilon_g = .5 T D^{.5} \times 10^{-4} \quad (2.41)$$

The viscosity and the thermal conductivity of the combustion gas are given by [89]

$$\eta = 2.1 \times 10^{-8} (T + 780) \quad (2.42)$$

$$\kappa = 1.71 \times 10^{-3} (T + 270)/M \quad (2.43)$$

In the calculation of the gas specific enthalpy, the value for chemical equilibrium is used (Appendix A) which implies that chemical equilibrium exists throughout the cross-section of the channel. This is justified by the short chemical reaction time (at high operating temperature and pressure), much smaller than the residence time of the gas in the combustion chamber.

2.5 Working Fluid Thermodynamic and Electrical Properties

The calculation of the thermodynamic properties of the gas are based on the law of mass action. This calculation involves the solution of a set of non-linear simultaneous differential equations for the molar concentration of the various fluid constituents. A number of numerical calculations have been performed by different researchers [96-99].

In this study, the thermodynamic equations were obtained from the equations developed by Baylis, et al. [100] to fit the detailed equilibrium calculation of Freck and Roberts [96]. The equations are given in Appendix A.

Since the combustion products of air and coal have a low electrical conductivity, an alkali metal (preferably cesium or potassium) is added to provide the necessary electrical conductivity for an efficient energy conversion. This process is called seeding. The degree of ionization (electron density) is given by the Saha equation [104]

$$\begin{aligned} \text{Log} \left[\frac{n_e n_i}{n_{os} - n_i} \right] &= - \frac{5040}{T} - \frac{3}{2} \text{Log} \frac{5040}{T} \\ &+ 26.9366 + \text{Log} 2 \frac{g_i}{g_o} \end{aligned} \quad (2.44)$$

which employs the law of mass action to obtain the electron density.

The calculation of the electron density and the electron mobility in a seeded plasma has been discussed by various authors [96-99, 101-103]. The electrical properties of the gas used in this study were obtained from the simplified equations developed in reference [100] to fit the detailed calculations performed by Freck [96]. The equations are given in Appendix A. It was assumed that the electrical properties of the gas may be represented by its average value throughout the cross-section of the channel. However in the presence of an electromagnetic field, a gasdynamic turbulence in the electrically conducting fluid gives rise to fluctuations in the electric field and current density. These arise because of the fluctuation in the velocity and conductivity of the working fluid. These changes in conductivity and electron mobility are subject to a time constant [105] which is sufficiently large so as to prevent the electrons from following the turbulent fluctuations.

The values of the electrical conductivity of the combustion products of coal, air and alkali seed still remain uncertain as long as the effects of the rare components of the coal and the slag are unknown. The effects may not be negligible. The uncertainty in the chemical composition of the seeded combustion gas makes it appropriate to use the simplified physical model for calculating the electrical conductivity.

2.6 The Single-load Segmented MHD Generator Model

The Linear MHD Generator. The linear channel is the simplest MHD generator geometry and by far the most advance in the development stage of MHD generators. In the following analysis, the velocity u is taken in the x -direction and the magnetic field in the z -direction. The induced electric field (yXB) is then equal to uB in the y -direction (See Figure 2.1).

The three most common types of linear generators are the continuous electrode Faraday, the segmented electrode Faraday and the segmented electrode Hall generator. The continuous electrode generator is shown in Figure 2.2. This is the simplest of all MHD generator configuration and operates with a single load. There is no electric field in parallel with the flow but large axial current exists.

The current density equations 2.30 and 2.31 become

$$J_x = -\sigma\beta_e (E_y - uB) / [(1 + \beta_i\beta_e)^2 + \beta_e^2] \quad (2.45a)$$

$$J_y = \sigma(1 + \beta_i\beta_e)(E_y - uB) / [(1 + \beta_i\beta_e)^2 + \beta_e^2] \quad (2.46a)$$

Introducing a load factor ($K = E_y/uB$), the above equations may then be written

$$J_x = \sigma\beta_e(1 - K)uB / [(1 + \beta_i\beta_e)^2 + \beta_e^2] \quad (2.45b)$$

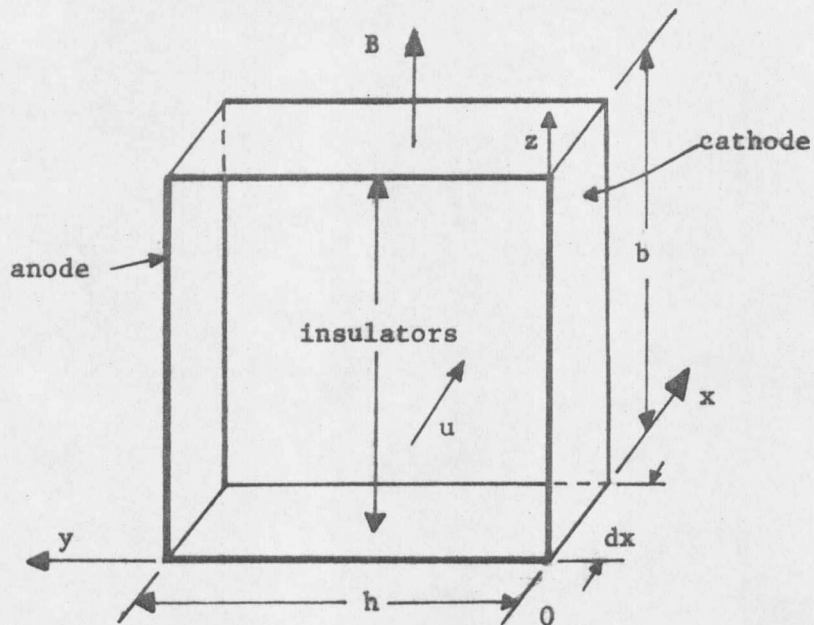


Figure 2.1 Diagram for the Analysis of a Linear MHD Generator

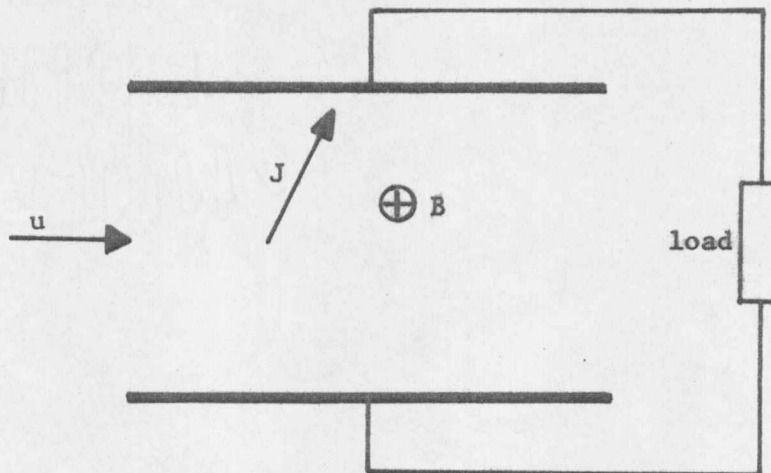


Figure 2.2 Continuous Electrode Faraday MHD Generator

$$J_y = \sigma(1 + \beta_i \beta_e)(K - 1)uB / [(1 + \beta_i \beta_e)^2 + \beta_e^2] \quad (2.46b)$$

The power generated per unit volume is

$$P = \underline{E} \cdot \underline{J} = \frac{\sigma K(1 - K)u^2 B^2 (1 + \beta_i \beta_e)}{(1 + \beta_i \beta_e)^2 + \beta_e^2} \quad (2.47)$$

since $E_x = 0$.

In the continuous electrode generator, there is an ohmic loss in the electrodes due to the axial current. This loss can be reduced by segmenting the electrodes (Figure 2.3). Thus, there is an axial field developed in the generator and the axial current becomes zero. The equations for this generator may then be written

$$J_y = \sigma(E_y - uB) / (1 + \beta_i \beta_e) \quad (2.48)$$

$$E_x = \beta_e (E_y - uB) / (1 + \beta_i \beta_e) \quad (2.49)$$

$$P = \underline{E} \cdot \underline{J} = K(1 - K)\sigma u^2 B^2 / (1 + \beta_i \beta_e) \quad (2.50)$$

The segmented electrode pairs may be short-circuited and an external load connected between the first and the last pair to form a Hall generator (Figure 2.4). This forces $E_y = 0$. Thus

$$J_x = \frac{\sigma[(1 + \beta_i \beta_e)E_x + \beta_e uB]}{(1 + \beta_i \beta_e)^2 + \beta_e^2} \quad (2.51)$$

$$E_x = - K_H \beta_e u B / (1 + \beta_i \beta_e) \quad (2.52)$$

where K_H is the ratio of the axial electric field and the open-circuit axial electric field. The power density is

$$P = E_x J_x = \frac{K_H (1 - K_H) \sigma \beta_e^2 u^2 B^2}{(1 + \beta_e \beta_i) [(1 + \beta_i \beta_e)^2 + \beta_e^2]} \quad (2.53)$$

An analysis of the Hall effect on the electrical terminal characteristics, generated power per unit volume, pressure gradient and generator efficiency [106] showed that for a segmented electrode Faraday generator, the performance is independent of the Hall effect for very small values of β_i . For the continuous electrode generator, the power density deteriorates as β_e increases. In a Hall generator, the power density and efficiency is comparable to that of the segmented electrode Faraday generator for large values of β_e ($\beta_e \gg 1$) but is poor when β_e is small. Analysis of the performance of these three types of generators with Hall effect was also performed by Celinski [40] with similar results.

For the working fluid under consideration in this study (combustion products of coal, air and seed), the Hall parameter β_e is in the range 1 to 6 and $\beta_i \ll 1$. This range of values rules out the use of the Hall and the continuous electrode generator. However,

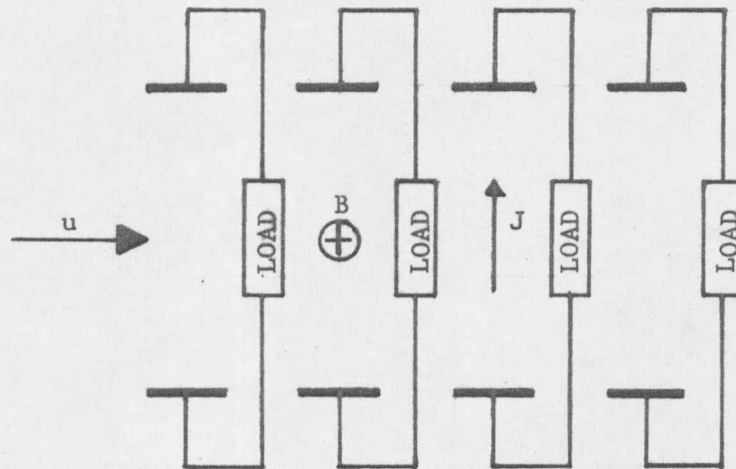


Figure 2.3 Segmented Electrodes Faraday Generator

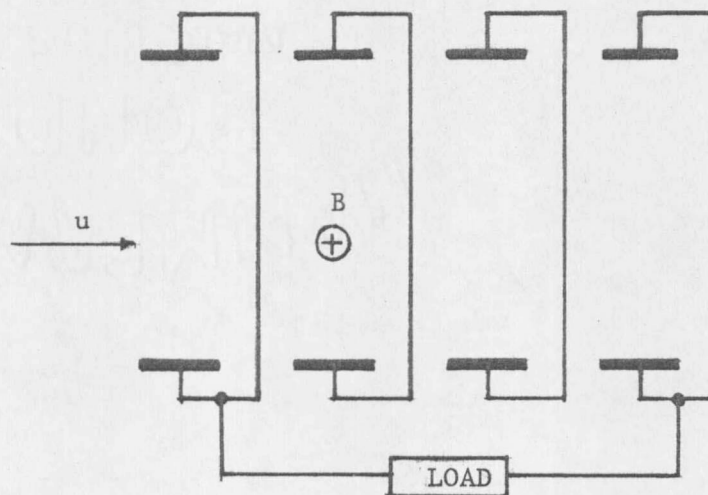


Figure 2.4 Hall Generator

the segmented electrode Faraday generator requires a multiplicity of loads.

A series or cross-connected generator was proposed by De Montardy [107] to reduce the number of loads connected to the generators (see Figure 2.5). The cross-connection between the electrodes on the top and bottom wall are equipotential planes in the one-dimensional model. The angle of cross-connection introduces an electrical constraint represented by the parameter

$$\alpha = -E_y/E_x \quad (2.54)$$

For a finely (infinite segmentation) segmented generator with no distortion in the electric field, the current density is [107]

$$J = J_x - \alpha J_y \quad (2.55a)$$

$$\text{or } J = (1 + \alpha^2) \sigma \frac{(E_x + uB(\alpha - \beta_e)/(1 + \alpha^2))}{(1 + \beta_e^2)} \quad (2.55b)$$

The open-circuit electric field is

$$E_{xo} = -uB(\alpha - \beta_e)/(1 + \alpha^2) \quad (2.56)$$

and the power density

$$P = JE_x = \frac{[K(1 - K)(\alpha - \beta_e)^2 \sigma u^2 B^2]}{(1 + \beta_e^2)(1 + \alpha^2)} \quad (2.57)$$

The total current crossing any equipotential plane is constant and equals the load current given by

$$I_L = A (J_x - \alpha J_y) \quad (2.58)$$

The generator electrical parameters (Lorentz force, current density, electric field, isentropic efficiency, power density) may be calculated in terms of the load current and the cross-connection parameter (α). Following Bayliss [100], a dimensionless current is introduced to simplify the equations (Table 2.1),

$$I^* = I_L / (A \sigma u B) \quad (2.59)$$

Isentropic efficiency is defined as the ratio of the power output per unit volume to the work done against the Lorentz force ($\underline{JXB}$).

$$\eta_e = (E_x J_x + E_y J_y) / J_y u B \quad (2.60)$$

Electrode Voltage Drop. There is an electrode voltage drop caused by the poor emission properties of the electrode materials and the cold boundary layer at the surface of the electrodes. The voltage drop varies with the load current, temperature and the material of the electrode walls. The higher the temperature of the electrode, the thinner is the boundary layer and the lower is the voltage drop [108]. At large current densities, the voltage drop is approximately constant.

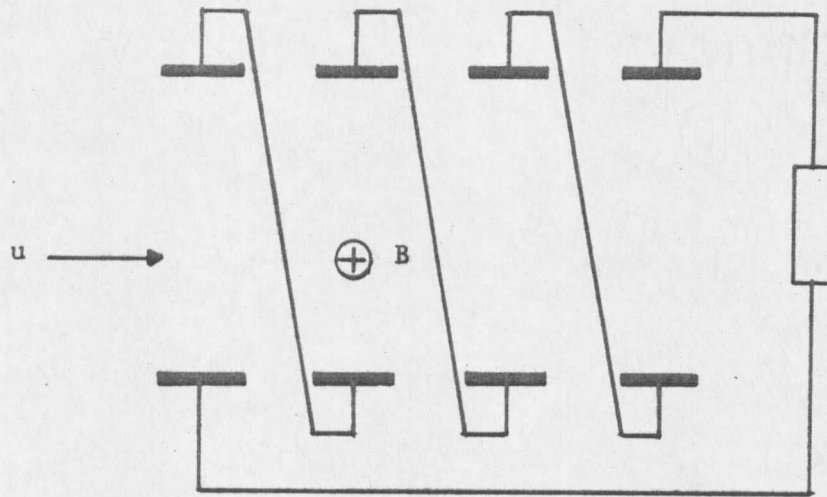


Figure 2.5. Series or Cross-connected Generator

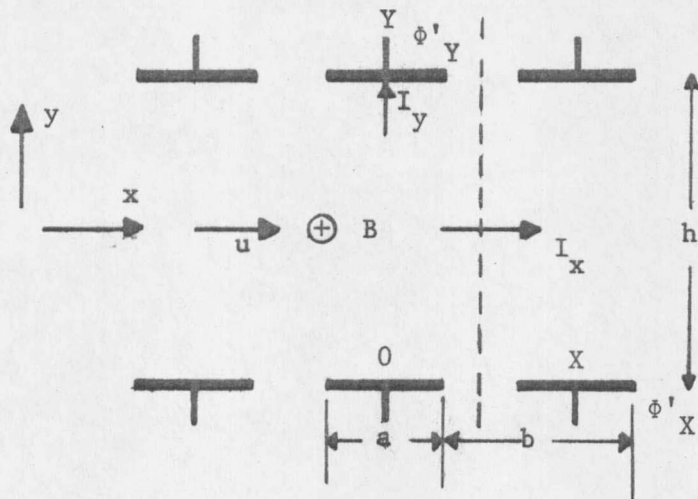


Figure 2.6. Coordinate System for Segmented Electrode Generator [100]

For a large MHD generator operating at subsonic condition the voltage drop may vary between 40 to 200 volts [110]. It is convenient to define an electrode voltage drop parameter

$$\phi = V/huB \quad (2.61)$$

and introduce this parameter in the one-dimensional equations for the single-load MHD generator.

Effects of Segmentation. The segmentation of the electrodes is necessary to compensate for the power reduction due to the Hall effect and also to reduce the duct end losses due to loop currents [111]. The analysis in the preceding section assumed infinite segmentation. In a practical MHD generator, the electrode pitch and width is finite, typically one to five centimeters, limited by the axial electric field. The influence of a finite electrode pitch and width (two-dimensional effects considered) for a segmented Faraday generator was first investigated by Hurwitz, Kilb and Sutton [112] and for the three types of generators (segmented Faraday, Hall and cross-connected generators) with the electrodes and the insulating gap equal in width by Dzung [113]. Similar investigations limited to the segmented Faraday generator were also performed by Yeh and Sutton [114] and Schultz-Grunow and Denzel [115]. In a later paper, Dzung [116] considered the influence of a finite electrode pitch for the three types of generators for any

ratio of electrode and gap widths. A set of curves and formulae were derived showing the effects of finite segmentation on the power density, generator efficiency and on the voltage-current characteristics of the MHD generator. An analysis performed by Witalis [117-118] also showed results similar to that obtained by Dzung.

A more useful form of introducing the two dimensional effects of electrode and gap widths into the one dimensional MHD equations based on the work of Dzung was proposed by Welly [100]. It is assumed that the conditions in the duct do not vary with x , thus the current and potential systems are periodic with the segmentation. Then, the currents and potentials of the three electrodes (0,X,Y) in Figure 2.6 may be expressed as functions of the Hall parameter, pitch to height ratio (b/h) and the electrode to pitch ratio (a/b). These are represented by resistances R_{xx}, R_{yx}, R_{yy} .

$$\Phi_{0X} = R_{xx} I_x + R_{xy} I_y \quad (2.62)$$

$$\Phi_{0Y} = R_{yx} I_x + R_{yy} I_y \quad (2.63)$$

where Φ - potentials measured in the moving frame at electrodes

X and Y with respect to electrode 0.

I_x - axial current

I_y - current to each electrode

$R_{xy} = -R_{yx}$, since there is no relative displacement between electrodes.

Table 2.1. Expression for the Generator Parameters [100]

Lorentz Force	$\underline{J \times B}$	$-\frac{\sigma u B^2 (1 + I^* (\alpha - \beta))}{1 + \alpha^2}$
Power Density	$\underline{J \cdot E}$	$-\frac{\sigma u^2 B^2 (I^* (\alpha + \beta) - I^{*2} (1 + \beta^2))}{1 + \alpha^2}$
Axial Current Density	J_x	$\frac{\sigma u B (I^* (1 + \alpha \beta) - \alpha)}{1 + \alpha^2}$
Transverse Current Density	J_y	$-\frac{\sigma u B (1 + I^* (\alpha - \beta))}{1 + \alpha^2}$
Axial Electric Field	E_x	$-\frac{u B [(\alpha + \beta) - \frac{I^* (1 + \beta^2)}{1 + \alpha^2}]}{1 + \alpha^2}$
Transverse Electric Field	E_y	$\frac{\alpha u B [(\alpha + \beta) - \frac{(1 + \beta^2) I^*}{1 + \alpha^2}]}{1 + \alpha^2}$
Isentropic Efficiency	η_e	$\frac{I^* (\alpha + \beta) - I^{*2} (1 + \beta^2)}{(\alpha - \beta) I^* + 1}$
Load Factor	K	$1 - \frac{(1 + \beta^2) I^*}{(\alpha + \beta)}$

The above equations may be written [100]

$$\sigma \bar{E}_x = k \bar{J}_{x-x} + k\beta \bar{J}_{-y} \quad (2.64)$$

$$\sigma \bar{E}_y = k \bar{J}_{y-y} + k\beta \bar{J}_{-x} \quad (2.65)$$

where the bars over the electric field and current density components denote that these are average values over the duct height or along one period of the segmentation (whichever is applicable) and the coefficients k_x , k_y , k are related to the resistances in equations 2.62 and 2.63.

The equations in Table 2.1 may then be modified by the inclusion of these coefficients and the electrode voltage drop parameter (ϕ) (see Table 2.2). The coefficients may be evaluated using numerical relaxation method [119-120] or by conformal mapping [112,116-118]. The coefficients used in this study were obtained from the performance curves published by Dzung [116] which is applicable to any configuration of electrodes and insulators in a generator operating under any load condition assuming homogeneity in the center of the duct. The curves are shown in Figure 2.7. The coefficients may then be written [100]

$$k_x = 1/(1 + b^* v) \quad (2.66)$$

$$k_y = 1 + b^* [(u + u')\beta + v'] - b^* u'^2/(1 + b^* v) \quad (2.67)$$

$$k = (\beta - b^* (u - v\beta))/(\beta(1 + b^* v)) \quad (2.68)$$

Table 2.2. Generator Parameters With Segmentation Effects [100].

$\bar{JXB}$	$-\frac{(1 + \phi)I^{*2}(k_x\alpha - k\beta)}{k_y + k_x\alpha^2} \sigma u B^2$
$\underline{J \cdot E}$	$-\frac{I^*(k_x\alpha + k\beta)(1 - \phi) - I^{*2}(k_x k_y + \beta^2 k^2)}{k_y + k_x\alpha^2} \sigma u B^2$
J_x	$\frac{I^*(k_y + k\alpha\beta) - k_x\alpha(1 - \phi)}{k_y + k_x\alpha^2} \sigma u B$
J_y	$-\frac{[(1 - \phi) + I^*(k_x\alpha - k\beta)]}{k_y + k_x\alpha^2} \sigma u B$
E_x	$-\frac{[(k_x\alpha + k\beta)(1 - \phi) - (k_x k_y + \beta^2 k^2)I^*]}{k_y + k_x\alpha^2} u B$
E_y	$\frac{\alpha k_x [(k_x\alpha + k_y\beta)(1 - \phi) - (k_x k_y + \beta^2 k^2)I^*] + \phi}{k_y + k_x\alpha^2} u B$
K	$1 - \frac{(k_x k_y + \beta^2 k^2)I^*}{(1 - \phi)(k_x\alpha + k\beta)}$
η_e	$\frac{I^*(k_x\alpha + k\beta)(1 - \phi) - I^{*2}(k_x k_y + \beta^2 k^2)}{(k_x\alpha - k\beta)I^* + (1 - \phi)}$

where $b^* = b/h$

$c = a/b$

a - electrode width

b - electrode pitch

h - electrode separation

and u, u', v, v' are obtained from the curves shown in Figure 2.7.

The derivation of the quasi one-dimensional equations this section were based on the assumption that all the MHD duct parameters (σ, β, u, B) are constant along the generator length. However, the parameters may vary in all directions thus introducing errors in the results. The electrodynamic effects of non-uniformity of magnetic field and fluid parameters was investigated by Coney [121]. The assumptions he made were (a) the fluid parameters are constant over the cross-section of the channel and (b) the effect of finite segmentation was neglected.

Coney used the relaxation method to solve the two-dimensional MHD duct equations with consideration of the effects of discontinuities. The result of his investigation showed that the one-dimensional method was adequate for the calculation of power output, electromagnetic pressure gradient and the total load current in the duct away from the discontinuity. However the one-dimensional theory could not adequately describe the potential distribution through a discontinuity, but

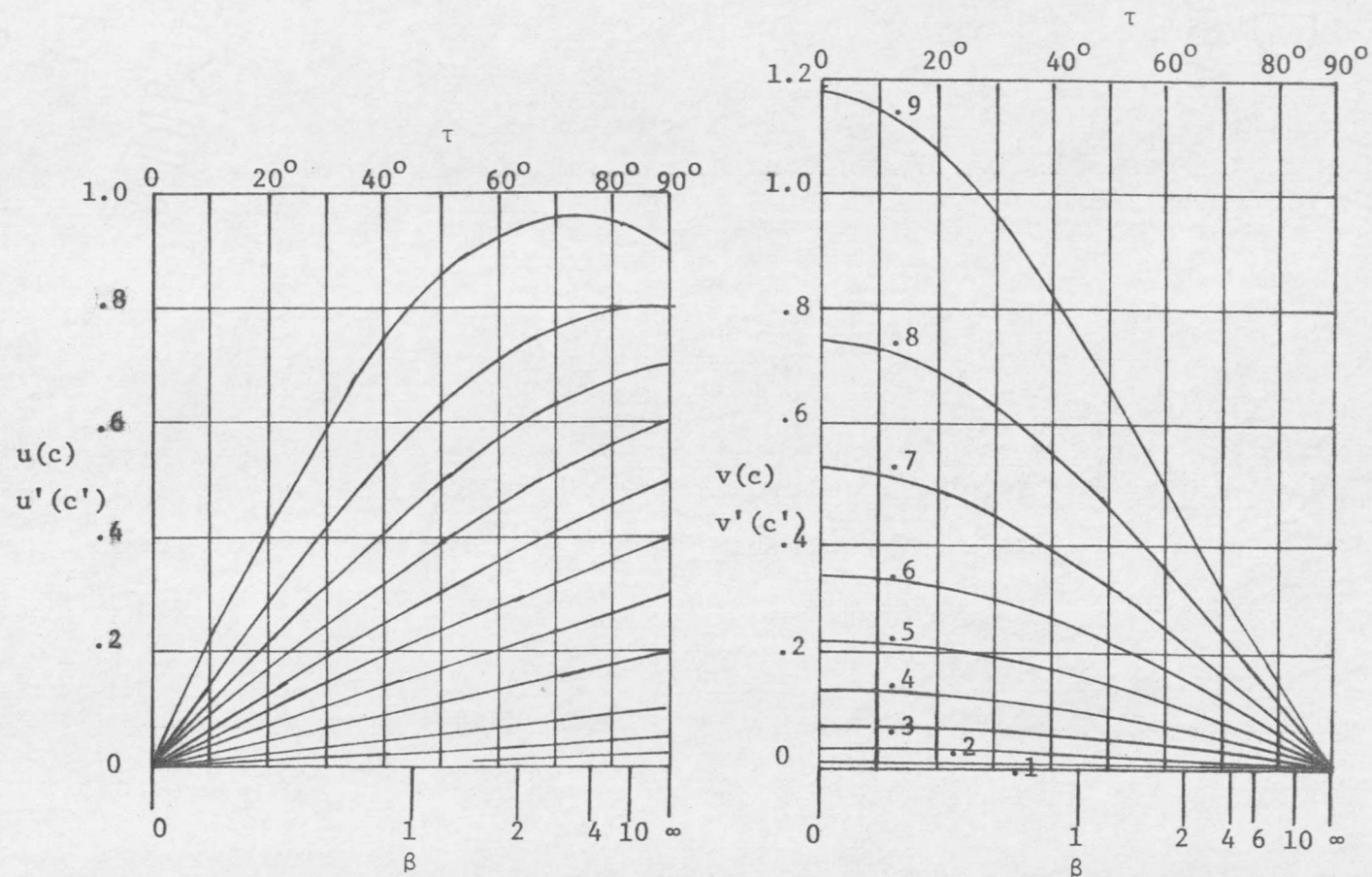


Figure 2.7. Correction factors u, u', v, v' [116]

the errors in the calculation of the power output and electromagnetic pressure over the region were not excessive.

Coney showed that for a small generator (150 MW thermal input), the errors obtained in the one-dimensional theory were small ($\pm 1\%$) in the calculation of the electromagnetic pressure both near and away from the discontinuity. The errors in the power output calculation were greater; over the whole duct, the power output calculated was about 3 % too high.

No study has been made as to the applicability of these results (or the scaling) to a large MHD generator operating under different magnetic field strength, pressure and temperature.

2.7 Boundary Layer Effects

The flow in the MHD generator may be divided into two parts, the inviscid core flow and the boundary layers in the vicinity of the duct walls. Several analyses have been performed to investigate this type of flow [38,122-123]. In reference [138-139], it was assumed that the viscous and thermal boundary layers have the same thickness (δ),

$$\delta(x) = a \cdot x \cdot \text{Re}_x^{-.2} \quad (2.69a)$$

In aerodynamics [124], the value of the coefficient (a) is .37, however in MHD generators the coefficient may be taken to be equal to .32 [122]. Thus

$$\delta(x) = .32 \cdot x^{.8} (u_p/\eta)^{-.2} \quad (2.69b)$$

The effects of the boundary layer are determined by the ratios (δ/b) and (δ/h) , where b and h are the cross-sectional dimensions of the MHD generator. To show the relative effects of the boundary layer on two different generator sizes, a parameter γ is introduced.

$$\gamma = \frac{(\delta_s/h_s) \text{ small generator}}{(\delta_l/h_l) \text{ large generator}} \quad (2.70a)$$

Assuming a square and constant cross-section for both generators and a factor of (K_l) in their linear dimensions, the relative effects (γ) at the half length of both channels is equal to

$$\gamma = K_l^{.2} [(u_s \rho_s / \eta_s) / (u_l \rho_l / \eta_l)]^{.8} \quad (2.70b)$$

If the velocity, the mass density and the temperature of the gas at the half-length of both channels have the same values, then

$$\gamma = K_l^{.2} \quad (2.70c)$$

A numerical example would be appropriate. Consider an experimental generator with the dimension (.1 m x .1 m x 1 m) and a large generator (1 m x 1 m x 10 m) with the gas parameters at the half-length ($u = 700$ m/sec; $\rho = .35$ Kg/m³; $T = 2400^\circ\text{K}$).

The boundary layer thickness for the generators are:

For the small generator $\delta = .00894$ m

For the large generator $\delta = .0564$ m

and the (δ/h) ratios are:

For the small generator $\delta/h = .0894$

For the large generator $\delta/h = .0564$

The ratio of the relative effects is $\gamma = K_1^{.2} = 1.58489$.

A theoretical investigation of the effects of the gas non-uniformity in the MHD channel due to the boundary layer was conducted by Celinski [46]. It was assumed that (a) the gas dynamic and electrical parameters are constant in the direction of the flow, (b) the effects of finite electrode width were neglected, (c) the electrical conductivity is a function of the local gas temperature and pressure only, (d) the channel is of constant cross-section, (e) the applied magnetic field is constant, (f) the magnetic Reynolds number is very small, (g) the channel end effects were negligible and (h) perfect electrodes and insulators were used. Celinski used the method of electrical equivalent circuits [125] in the calculation. Only the solution to the two-dimensional problem was obtained due to the large computer capacity requirement of a three-dimensional model. The results of Celinski's study showed that (a) the open-circuit voltage is not strongly affected by the boundary layer and its magnitude is slightly dependent on the wall temperature and independent of the Hall parameter. (b) The internal resistance of the generator is strongly increasing with the increase of the Hall parameter, especially when cold walls are used. (c) The power output is strongly

influenced by the boundary layer thickness. (d) The Ohmic losses due to the circulating current increase with the increase in the boundary layer thickness and with the decrease of the Hall parameter. (e) The axial current increases with the increase in the thickness of the boundary layer.

2.8 Scope and Limitations of the Model

The quasi one-dimensional model used in this study is based on the assumptions presented in section 2.3, applicable to large MHD generators. The heat transfer and friction terms used are discussed in section 2.4. It is assumed that the deposition of the slag and the seed on the inner walls of the channel would result in the reduction of the heat transfer coefficient. The friction coefficient for a large MHD generator is assumed to be smaller than that of a small experimental generator but larger than that of the smooth tube. The size of the generator considered is large enough such that the overall effect of the boundary layer is a reduction in the power output by less than 10 %.

Since the calculation of the electrical conductivity is based on the simplified equations (Appendix A) for the combustion products of carbon, hydrogen, oxygen and nitrogen and seed, the calculated conductivity may deviate from the actual conductivity by as much as 10 %.

The effect of the electrode voltage drop and the finite segmentation are considered to improve the overall accuracy of the model.

The non-uniformities in the end regions are neglected. However, for a large generator, the aspect ratio (L/D) can be chosen such that the power losses in the end regions are minimized. Furthermore, the exponential decay of the magnetic field at the end regions tend to reduce the eddy and circulating currents.

A single-load MHD generator is considered to avoid the multiplicity of load required by the segmented Faraday generator. The gas-dynamic input conditions are chosen to represent operating conditions applicable to large MHD generators.

Chapter III

FORMULATION OF THE MATHEMATICAL MODEL OF THE MHD POWER GENERATION SYSTEM COMPONENTS

3.0 Introduction

The mathematical model of the MHD system components are developed in this chapter. The components considered are the steam bottoming plant, the air heater and the DC to AC converter. The description of the gas and the steam cycles are presented in section 3.1. The steady state air, gas and steam parameters are also given. The dynamic model for the steam plant and the air heater are formulated in section 3.2. In section 3.3, the dynamic model of the DC to AC converter is described.

3.1 The Steam Bottoming Plant and The Air Heater

The block diagram of the MHD-Steam power generating system considered in this study is shown in Figure 3.1. The steam plant and the air heater are partitioned into the following sections:

(a) Water/Steam Cycle

- (1) Feedwater heater
- (2) Feedwater pumps
- (3) Wall cooling - combustor, MHD generator and diffuser
- (4) Economizer
- (5) Radiant section

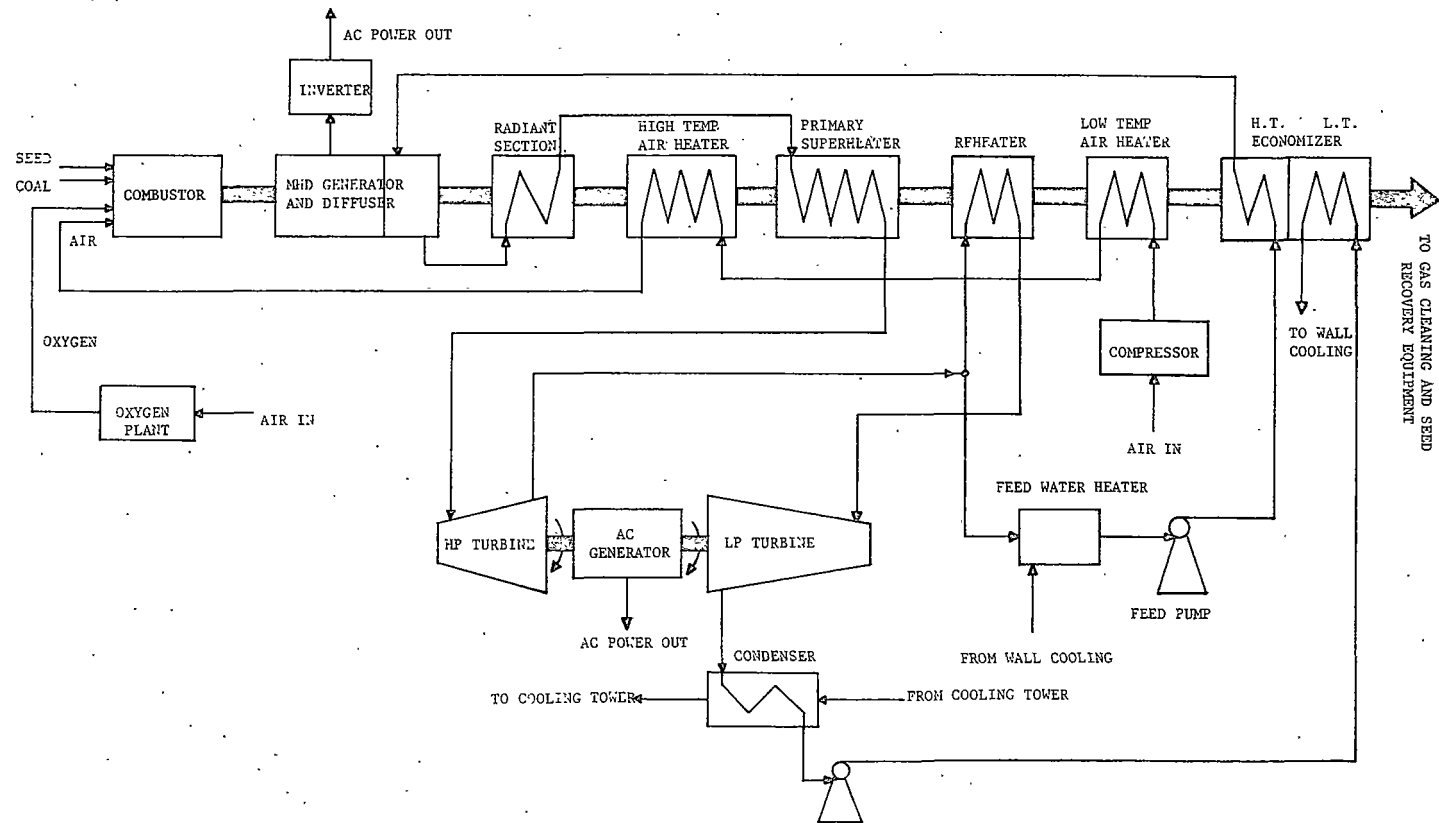


Figure 3.1. MHD/Steam Electrical Power Generation System

- (6) Primary Superheater
 - (7) High Pressure Steam Turbine
 - (8) Reheater
 - (9) Low Pressure Steam Turbine
 - (10) Condenser
 - (11) Extraction Pump
- (b) Air cycle
- (1) Compressor
 - (2) Low Temperature Air Heater
 - (3) High Temperature Air Heater

As a first step towards the simulation of a complete MHD-Steam power generation system, a simple open-loop model of the steam plant and the air heater is considered. The components included in the model are the high temperature economizer, the low temperature air heater, the radiant section, the high temperature air heater, the primary superheater, and the reheater. The effect of the low temperature economizer, the feedwater heater, the wall cooling tubes and the feedwater pump dynamics were omitted because the added complexity would not result in any significant contribution to the model performance.

3.1.1 The Steam Cycle

The top steam temperature used in the heat balance calculations is 823°K. Supercritical pressure with single reheat is used. The

complete steam cycle is shown in Figure 3.2, with the corresponding inlet and outlet steam conditions. The steam plant design is based on a 2000 MW thermal input to the MHD combustion chamber.

3.1.2 The Gas Path

From the MHD duct, the combustion gas flows through the diffuser where efficient pressure recovery is very important. The heat loss in the diffuser is employed in producing steam. The gas then flows through the radiant section consisting of a rectangle formed by tubes contained in a lined cylindrical pressure vessel. Approximately 95 % of the heat is absorbed by the tubes through radiation and 5 % through convection (determined by the convection and radiation heat transfer coefficients and the gas temperature [74,95,127]). The next section is the regenerative high temperature air heater where the heat transfer rate is approximately 93 % by radiation and 7 % by convection. The gas leaving the high temperature air heater flows through the primary superheater where the heat transfer rate is approximately 10 % by radiation and 90 % by convection. However in the simplified model the heat transfer rate is assumed to be purely by convection. The reheater section is downstream of the primary superheater. The gas then flows through the low temperature air heater and finally through the two economizers. The complete gas path is shown in Figure 3.3. The important design parameters of the steam plant and the air heaters are shown in Appendix B.

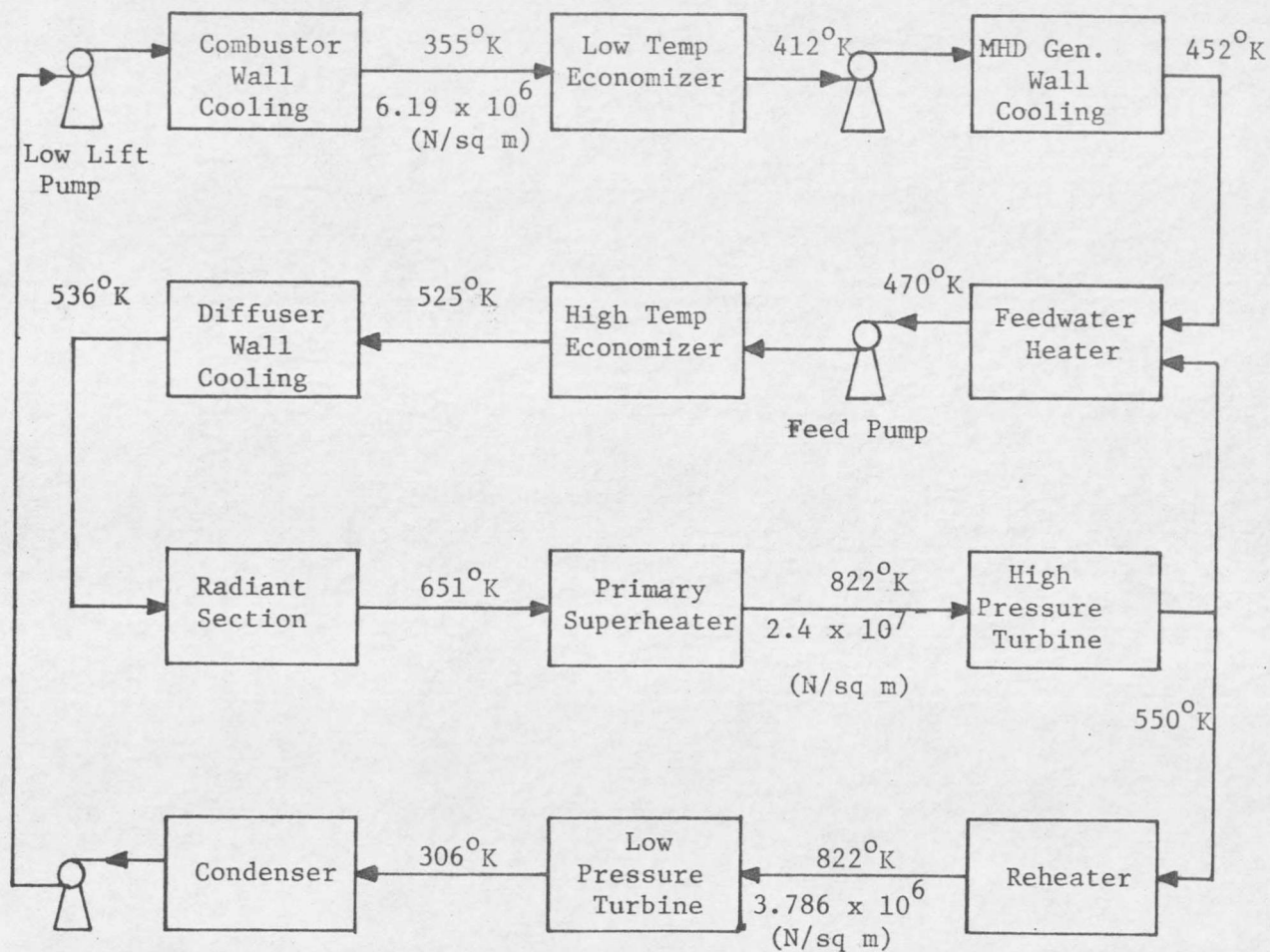


Figure 3.2. Water/Steam Cycle - Steam Bottoming Plant

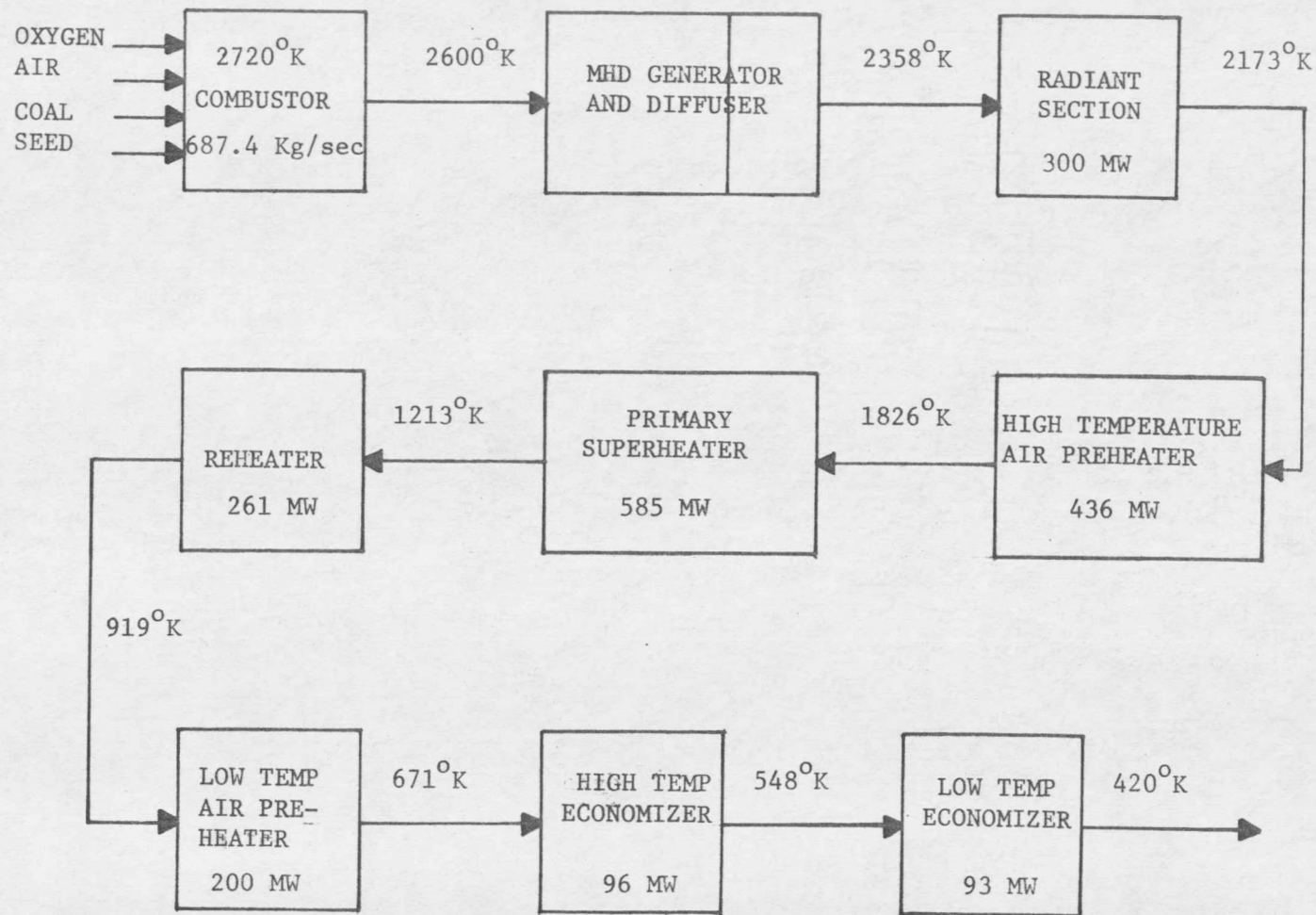


Figure 3.3. Gas Path - MHD/Steam Electrical Power Generation System

3.2 Formulation of the Mathematical Model of the Steam Plant and The Air Heater

The dynamic behavior of the steam plant and the air heater may be represented by non-linear, partial differential equations related to fluid flow and heat transfer. The solution of a set of these equations are difficult, thus simplifying assumptions have to be made. The steam plant is divided into a number of sections such that the properties of steam, water and the combustion gas are assumed to vary only in the axial direction. The properties of steam in each section may be represented accurately by a polynomial derived from the steam tables [126] using the least squares method [Appendix C]. This reduces the number of calculations required (by approximately 80 %) to obtain the necessary steam properties. Since the fluid is at supercritical conditions, there is no distinct transition from water to steam. The basic assumptions made in the formulation of the mathematical model are:

- (a) Fluid flow is one-dimensional fully developed turbulent flow.
- (b) Heat transfer from the tube wall to the internal fluid may be expressed by the Nusselt equation [127], which applies only to constant flow.
- (c) All gravitational and kinetic energy terms are neglected.
- (d) The heat transfer along the length of the tube or along

the length of the fluid flow is negligible.

(e) Heat storage of the tube is concentrated at the center of the tube wall.

(f) Fluid properties are defined by average values over the cross-sectional area.

(g) The dynamics of the combustion gas are negligible.

Every section of the steam plant consists of hundreds of tubes in parallel. Uniform distribution among the parallel flow is assumed, thus a single fluid flow may be used to represent the multiple flow path of each unit.

The film conductance of the steam is assumed to vary with the .8 power of the fluid flow inside the tube and the film conductance on the gas side is assumed to vary as the .6 power of the gas flow [95]. The variation of the coefficients with temperature is neglected. The gas-side radiative heat transfer is represented by the Stefan-Boltzman law [95]. The volumes and the weights of the connecting pipes are considered to be lumped with their adjacent section.

The steam plant is basically a Universal-Pressure boiler [127]. It is a high capacity, high temperature, once-through boiler. The working fluid is pumped into the unit as a liquid, flows through all the pressure-part heating surfaces sequentially, where it is converted to steam as it absorbs heat. There is no recirculation of water within the unit, thus a drum is not required to separate the water from

the steam. At supercritical pressure there is no distinction between the water and the steam.

Since the dynamic response of the turbine-generator combination are much faster than that of the boiler, only the steady state equations of the turbine are included in the model.

The dynamic equations for each section are based on the steam properties at the outlet of the section and the gas properties at the inlet of that section.

The mathematical model of each section described below is based on the governing laws of fluid dynamics (conservation of mass, conservation of momentum and conservation of energy), empirical heat transfer relationships and the equation of state obtained from standard textbooks [80,95,128].

Conservation of mass

$$A \frac{\partial \rho}{\partial t} + \frac{\partial w}{\partial x} = 0 \quad (3.1)$$

Conservation of Momentum

$$\frac{\partial p}{\partial x} + \rho \left(\frac{\partial u}{\partial t} + u \frac{\partial u}{\partial x} \right) + 2 C_f \frac{\rho u^2}{D} = 0 \quad (3.2)$$

Conservation of Energy

$$\frac{\partial}{\partial x}(wh) + A \frac{\partial}{\partial t}(U\rho) = q_w \quad (3.3)$$

Equation of State

$$\rho = \rho(p, h) \quad (3.4)$$

$$T = T(p, h) \quad (3.5)$$

Heat Storage in the Tube Wall

$$q_g - q_s = A_w \rho_w C_w \frac{\partial T}{\partial t} \quad (3.6)$$

Heat Transfer from the Tube Wall to the Fluid

$$q_s = C_g \pi D (w_s / w_{so})^{.8} (T_w - T_s) \quad (3.7)$$

Convective Heat Transfer from the Gas Side to the Tube Wall

$$q_g = C_g \pi D_o (w_g / w_{go})^{.6} (T_g - T_w) \quad (3.8)$$

Radiative Heat Transfer from the Gas Side to the Tube Wall

$$q_g = \pi D_o S (T_g^4 - T_w^4) \quad (3.9)$$

The partial differential equations representing the fluid dynamics may be reduced to ordinary differential equations valid only for small disturbances about an operating point.

The fluid dynamic equations may then be reduced to:

Conservation of mass

$$V \frac{d\rho}{dt} = w_i - w_o \quad (3.10)$$

Conservation of Momentum

$$p_i - p_o = f w_i^2 / \rho_i \quad (3.11)$$

Conservation of energy

$$w_i h_i + Q_s - w_o h_o = V \frac{d}{dt}(\rho h) \quad (3.12)$$

The heat transfer equations may be rewritten:

Heat Storage in the tube wall

$$Q_g - Q_s = W_w C_w \frac{dT_w}{dt} \quad (3.13)$$

Heat transfer from the Tube Wall to the Fluid

$$Q_s = \text{constant} \times w_s^{.8} (T_w - T_s) \quad (3.14)$$

Convective heat Transfer from Gas Side to the Tube Wall

$$Q_g = \text{constant} \times w_g^{.6} (T_g - T_w) \quad (3.15)$$

Radiative Heat Transfer from the Gas Side to the Tube Wall

$$Q_g = \text{constant} \times (T_g^4 - T_w^4) \quad (3.16)$$

3.2.1 The High Temperature Economizer

The feedwater leaving the wall cooling tubes (MHD duct and combustor) flows through the feedwater heater. The feedwater pump increases the water pressure from 850 psia to supercritical pressure

$(2.728 \times 10^7 \text{ Newtons/sq m})$. The feedwater then flows through the high temperature economizer. The economizer consists of 5.1 cm tubes arranged on a 10.4 cm square pitch.

Mass equation

$$w_{ei} - w_{eo} = V_e \frac{d\rho}{dt}_{eo} \quad (3.17)$$

Momentum Equation

$$p_{ei} - p_{eo} = f_e w_{ei}^2 / \rho_{ei} \quad (3.18)$$

Energy equation

$$w_{ei} h_{ei} - w_{eo} h_{eo} + Q_e = V_e \frac{d}{dt} (\rho_{eo} h_{eo}) \quad (3.19)$$

Heat Transfer equations

$$Q_{ge} - Q_e = W_e C_e \frac{dT}{dt}_{em} \quad (3.20)$$

$$Q_e = \text{constant} \times w_{ei}^{.8} (T_{em} - T_{eo}) \quad (3.21)$$

Steam (water) properties (See Appendix C)

$$\rho = \rho(p, h) \quad ; \quad T = T(p, h)$$

3.2.2 The Diffuser Wall Cooling Tubes

The water leaving the high temperature economizer is used to maintain the diffuser wall temperature at 1300° Kelvin. It is assumed

that the change in the wall temperature for a given change in the gas temperature would be very small, thus the metal heat balance equation is not included in this section. The steam flows through small tubes (2.635 cm OD) with adjacent tubes in contact.

Mass equation

$$w_{eo} - w_{do} = V_d \frac{d\rho}{dt} \quad (3.22)$$

Momentum equation

$$p_{eo} - p_{do} = f_d w_{eo}^2 / \rho_{eo} \quad (3.23)$$

Energy equation

$$w_{eo} h_{eo} + Q_d - w_{do} h_{do} = V_d \frac{d}{dt} (\rho_{do} h_{do}) \quad (3.24)$$

Heat transfer equation

$$Q_d = \text{constant} \times w_{do}^{.8} (T_w - T_{do}) \quad (3.25)$$

3.2.3 The Radiant Section

The radiant section is remote from the combustion chamber thus ensuring that no tubes can see the flame. The tubes (3.8 cm OD) are arranged with a tube pitch 3.8 cm, touching but not welded to adjacent tubes. Parallel flow is considered in this section. The equations

are:

Mass equation

$$w_{do} - w_{si} = V_p \frac{d\rho}{dt} si \quad (3.26)$$

Momentum equation

$$p_{do} - p_{si} = f_p w_{do}^2 / \rho_{do} \quad (3.27)$$

Energy Equation

$$w_{do} h_{do} + Q_p - w_{si} h_{si} = V_p \frac{d(\rho_{si} h_{si})}{dt} \quad (3.28)$$

Heat transfer equations

$$Q_{gp} - Q_p = W_p C_p \frac{dT_{pm}}{dt} \quad (3.29)$$

$$Q_p = \text{constant} \times w_{si}^{.8} (T_{pm} - T_{si}) \quad (3.30)$$

3.2.4 The Primary Superheater

The steam from the radiant superheater flows through the primary superheater in parallel flow arrangement. The tubes are divided into two banks to allow soot blowing. From the primary superheater the steam flows to the high pressure steam turbine. The turbine transforms the heat of the superheated steam into work without forming moisture. A portion of the steam leaving the turbine is used for feedwater heating.

The equations are:

Mass equation

$$w_{si} - w_{hi} = V_s \frac{d\rho_{hi}}{dt} \quad (3.31)$$

Momentum equation

$$p_{si} - p_{hi} = f_s w_{si}^2 / \rho_{si} \quad (3.32)$$

Energy equation

$$w_{si} h_{si} + Q_s - w_{hi} h_{hi} = V_s \frac{d(\rho_{hi} h_{hi})}{dt} \quad (3.33)$$

Heat transfer equations

$$Q_{gs} - Q_s = W_s C_s \frac{dT_{sm}}{dt} \quad (3.34)$$

$$Q_s = \text{constant} \times w_{hi}^{.8} (T_{sm} - T_{hi}) \quad (3.35)$$

3.2.5 The Reheater

The steam discharged from the high pressure turbine is reheated before being fed to the low pressure turbine. This provides a larger enthalpy change in the low pressure turbine resulting in an increase in efficiency.

Mass equation

$$w_{ho} - w_{li} = V_r \frac{d\rho_{li}}{dt} \quad (3.36)$$

Momentum equation

$$p_{ho} - p_{li} = f_r w_{ho}^2 / \rho_{ho} \quad (3.37)$$

Energy equation

$$w_{ho} h_{ho} + Q_r - w_{li} h_{li} = V_r \frac{d}{dt} (p_{li} h_{li}) \quad (3.38)$$

Heat transfer equations

$$Q_{gr} - Q_r = W_r C_r \frac{dT_{rm}}{dt} \quad (3.39)$$

$$Q_r = \text{constant} \times w_{li}^{.8} (T_{rm} - T_{li}) \quad (3.40)$$

3.2.6 The Steam Turbines

Since the dynamic response of the turbine is much faster than that of the boilers, only the steady state equations for these components are used. There are two turbines considered in this study, the high pressure turbine and the low pressure turbine. The intermediate pressure turbine is assumed to be lumped together with the low pressure turbine.

The High Pressure TurbineEnergy equation

$$w_{hi} h_{hi} - w_{ho} h_{ho} = P_h \quad (3.41)$$

where P_h represents the mechanical output of the turbine. The steam

flow rate is [128]

$$w_{hi} = A_h p_{hi} / \sqrt{T_{hi}} \quad (3.42)$$

where A_h , the flow area is controlled by the governor valve position.

The output to the shaft is directly proportional to the steam flow for small perturbations

$$P_h = \text{constant} \times w_{hi} \quad (3.43)$$

The Low Pressure Turbine

The equations are:

$$w_{li}^h - w_{lo}^h = P_1 \quad (3.44)$$

$$w_{li} = A_l p_{li} / \sqrt{T_{li}} \quad (3.45)$$

$$P_1 = \text{constant} \times w_{li} \quad (3.46)$$

3.2.7 The Low Temperature Air Heater

The low temperature air heater is a metallic , recuperative type with a thermal duty of 200 megawatts. The air is compressed by a turbine driven compressor to a pressure of eight atmospheres, then fed to the low temperature air heater. The heater raises the air temperature to 838°K.

The equations are:

Mass equation

$$w_{ac} - w_{al} = V_{al} \frac{d\rho_{al}}{dt} \quad (3.47)$$

Momentum equation

$$p_{ac} - p_{al} = f_{al} w_{ac}^2 / \rho_{al} \quad (3.48)$$

Energy equation

$$w_{ac} h_{ac} + Q_{al} - w_{al} h_{al} = V_{al} \frac{d}{dt} (\rho_{al} h_{al}) \quad (3.49)$$

Heat transfer equations

$$Q_{gal} - Q_{al} = W_{al} C_{al} \frac{dT_{alm}}{dt} \quad (3.50)$$

$$Q_{al} = \text{constant} \times w_{al}^{.6} (T_{alm} - T_{al}) \quad (3.51)$$

Properties of Air (See Appendix D)

$$Z = Z(p, h) \quad (3.52)$$

$$T = T(p, h) \quad (3.53)$$

$$\rho = p / (2.870678 \times 10^2 \times Z \times T) \quad (3.54)$$

3.2.8 The High Temperature Air Heater

A checker brick regenerator is proposed to heat the air from 838°K to 1480°K. (See Figure 3.4). The dynamic response of the

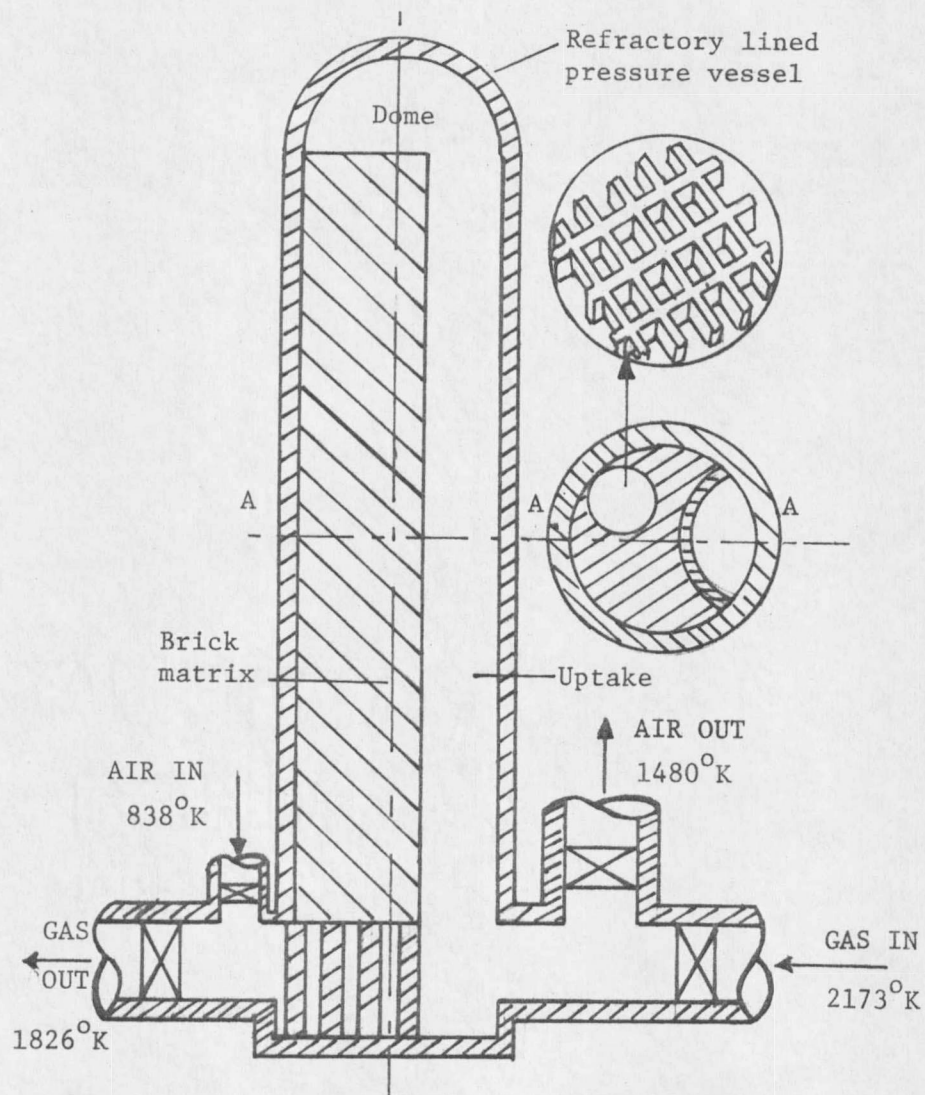


Figure 3.4. Sectional view of the checker brick regenerator unit [129]

regenerator may be determined from the thermal equations consisting of partial differential equations. The thermal equations are based on an equivalent circular channel of the brick [129]. (See figure 3.5). Following Chojnowski, et al. [129], the thermal equations are:

In the brick

$$\kappa_b \nabla^2 \theta = \rho C_b \frac{\partial \theta}{\partial t} \quad (3.55)$$

In the fluid (gas or air)

$$q 2\pi R_i = w C_p \frac{\partial T}{\partial z} \quad (3.56)$$

where $q = h_a (\theta_i - T)$ during the air blow and

$q = h_g (\theta_i - T) + \epsilon (\theta_i^4 - T^4)$ during the gas blow

The boundary conditions are

$$t \geq 0, \quad \kappa_b \nabla \theta = 0 \quad \text{at } r = R_o$$

$$\kappa_b \nabla \theta = q \quad \text{at } r = R_i$$

$$T(\text{inlet}) = \text{constant}$$

where a - width of the channel

b - channel pitch

C_b -- specific heat of the brick

ϵ - the radiation transfer coefficient, circular model

h - the convective heat transfer coefficient, circular model

κ_b - thermal conductivity of the brick, circular model

q - rate of heat transfer per unit cross-section, circular model

R - fixed radius, circular model, ($R_i = a/\sqrt{\pi}$; $R_o = b/\sqrt{\pi}$)

r - general radius, circular model

T - fluid temperature

t - time

w - mass flow rate of the fluid

ρ - brick temperature

θ - brick mass density

Suffixes a - air

g - gas

i - inner

o - outer

The result of the numerical solution of these thermal equations and an investigation of the temperature behavior of the fluids and the refractory materials used (conducted by Chojnowski, et al [129]) are shown in Figure 3.6. A conceptual design of the regenerator was also presented in reference [129]. For a 2000 MW thermal plant, the regenerator will consist of 12 units of which at any time 6 will be on gas, 3 on air, 2 on pressurization and depressurization during switchover and one on standby. Since the time response of the brick regenerator is longer compared to the metallic tubes in the steam plant, a steady state flow is assumed for this section.

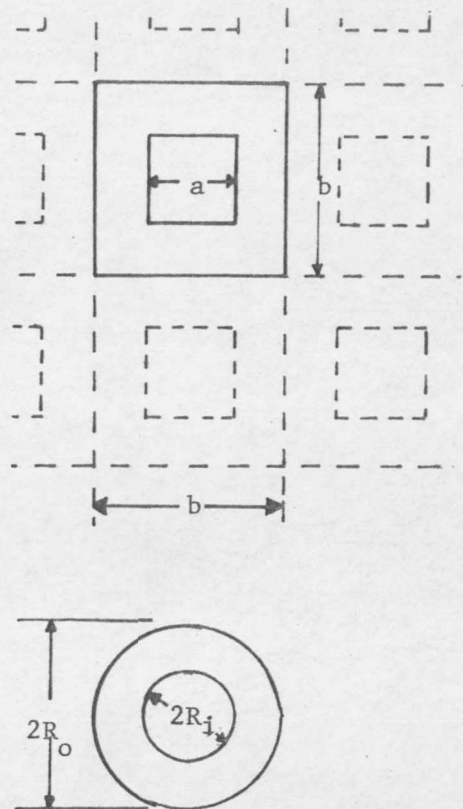


Figure 3.5. Model used in the numerical analysis [129]

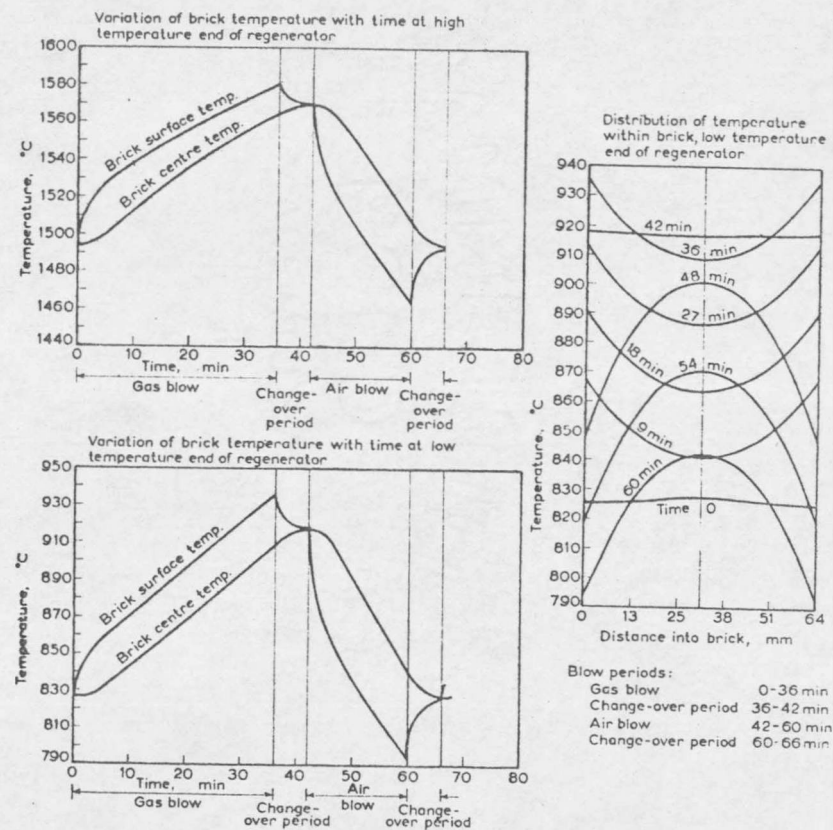


Figure 3.6. Brick regenerator matrix elements, temperature-time variations [129]

3.2.9 The Gas Path

The fuel used has a low heating value such that oxygen enrichment is necessary to obtain high thermal efficiency. The fuel is seeded with potassium salt (K_2CO_3) to provide the necessary electrical conductivity. With the air pre-heated to $1480^\circ K$ and enriched with oxygen to produce a Nitrogen/Oxygen mole ratio of 3.0, the combustion temperature is approximately $2700^\circ K$ at 5.5 atmospheres of pressure. The equations used in the calculation of the combustion temperature are shown in Appendix F. The combustion gas is accelerated through a nozzle in order to attain a velocity of 765 meters/sec at the generator inlet. The gas flows through the MHD generator where a portion of the kinetic and the thermal energy is converted to electrical energy in the presence of a constant applied magnetic field. Pressure recovery and velocity reduction is obtained through the diffuser. From the diffuser the gas flows through the steam plant and the air heaters. In the following equations for the gas path, the inertia and the heat capacitance of the gas are neglected.

The heat transfer equations are:

$$Q_{gp} = \text{constant} \times (T_g^4 - T_{pm}^4) + \text{constant} \times w_g^{.6} (T_g - T_{pm}) \quad (3.57)$$

$$Q_{gah} = \text{constant} \times (T_{gp}^4 - T_b^4) + \text{constant} \times w_g^{.6} (T_{gp} - T_b) \quad (3.58)$$

$$Q_{gs} = \text{constant} \times w_g^{.6} (T_{gah} - T_{sm}) \quad (3.59)$$

$$Q_{gr} = \text{constant} \times w_g^{.6} (T_{gs} - T_{rm}) \quad (3.60)$$

$$Q_{gal} = \text{constant} \times w_g^{.6} (T_{gr} - T_{alm}) \quad (3.61)$$

$$Q_{ge} = \text{constant} \times w_g^{.6} (T_{gal} - T_{em}) \quad (3.62)$$

$$T_{gp} = T_g - Q_{gp} / (C_{gp} w_g) \quad (3.63)$$

$$T_{gah} = T_{gp} - Q_{gah} / (C_{gah} w_g) \quad (3.64)$$

$$T_{gs} = T_{gah} - Q_{gs} / (C_{gs} w_g) \quad (3.65)$$

$$T_{gr} = T_{gs} - Q_{gr} / (C_{gr} w_g) \quad (3.66)$$

$$T_{gal} = T_{gr} - Q_{gal} / (C_{gal} w_g) \quad (3.67)$$

$$T_{ge} = T_{gal} - Q_{ge} / (C_{ge} w_g) \quad (3.68)$$

The specific heat of the gas is dependent on the gas pressure and gas temperature. The specific heat values shown in Appendix E were obtained from the gas properties calculations (Appendix A).

The equations used in the calculation of the combustion temperature for a given air pre-heat temperature and pressure, oxygen enrichment and seeding level are shown in Appendix F.

3.3 Dynamic Model of the DC to AC Converter

The direct current (DC) produced by the MHD generator is converted to alternating current (AC) and stepped up in voltage for transmission compatible with an existing network. DC to AC power conversion has been successfully employed in high-voltage systems in several countries [130-131].

Two types of valves have been used in these high voltage systems, mercury arc valves and thyristors. The state of the art has progressed to the point that thyristors can be used in high voltage DC systems [132-136].

The voltage-current characteristics of a MHD generator have been predicted and experimentally verified to be a straight line, from open circuit voltage and zero current to zero voltage and short circuit current (assuming that the gas dynamic parameters remain the same). For varying load conditions, the generator may operate on a single voltage-current curve and the inverter circuit would then be required to maintain constant voltage as required by the transmission line. The DC to AC converter would perform the following functions, (a) it must stabilize the system in cases of deviations in the working fluid parameters, (b) it must facilitate equipment operation in change-over

and post emergency conditions and (c) it must provide for the proper regulation of the MHD generator power in conjunction with the external grid system. Thus, the DC to AC power converter should include control circuitry with the following basic features, (a) limit the fluctuation of the current due to the fluctuation of the alternating voltages, (b) limit the maximum current (in cases of abnormal conditions) to avoid damage to the inverter valves and other current carrying devices, (c) prevent commutation failures in the inverter, (d) keep the power factor as high as possible, (e) control the power delivered, (f) control the frequency and (g) reduce the harmonics.

The proposed system which includes the above features is shown in Figure 3.7. The control of the power delivered and the limitation of the fluctuation of current and maximum current are accomplished by the use of the tap-changer control of the transformer and the control of the ignition angle of the valves. To prevent commutation failure, constant extinction angle control may be employed. This method is thoroughly discussed in reference [130]. Harmonics are reduced by the use of harmonic filters consisting of capacitors and inductors in series or parallel. Power factor is maintained as high as possible by adding shunt capacitors and also by making the extinction angle as small as possible. Frequency control is accomplished by controlling the ignition angle of the inverter valves. A detailed information on the design of these control mechanisms can be found in reference [130].

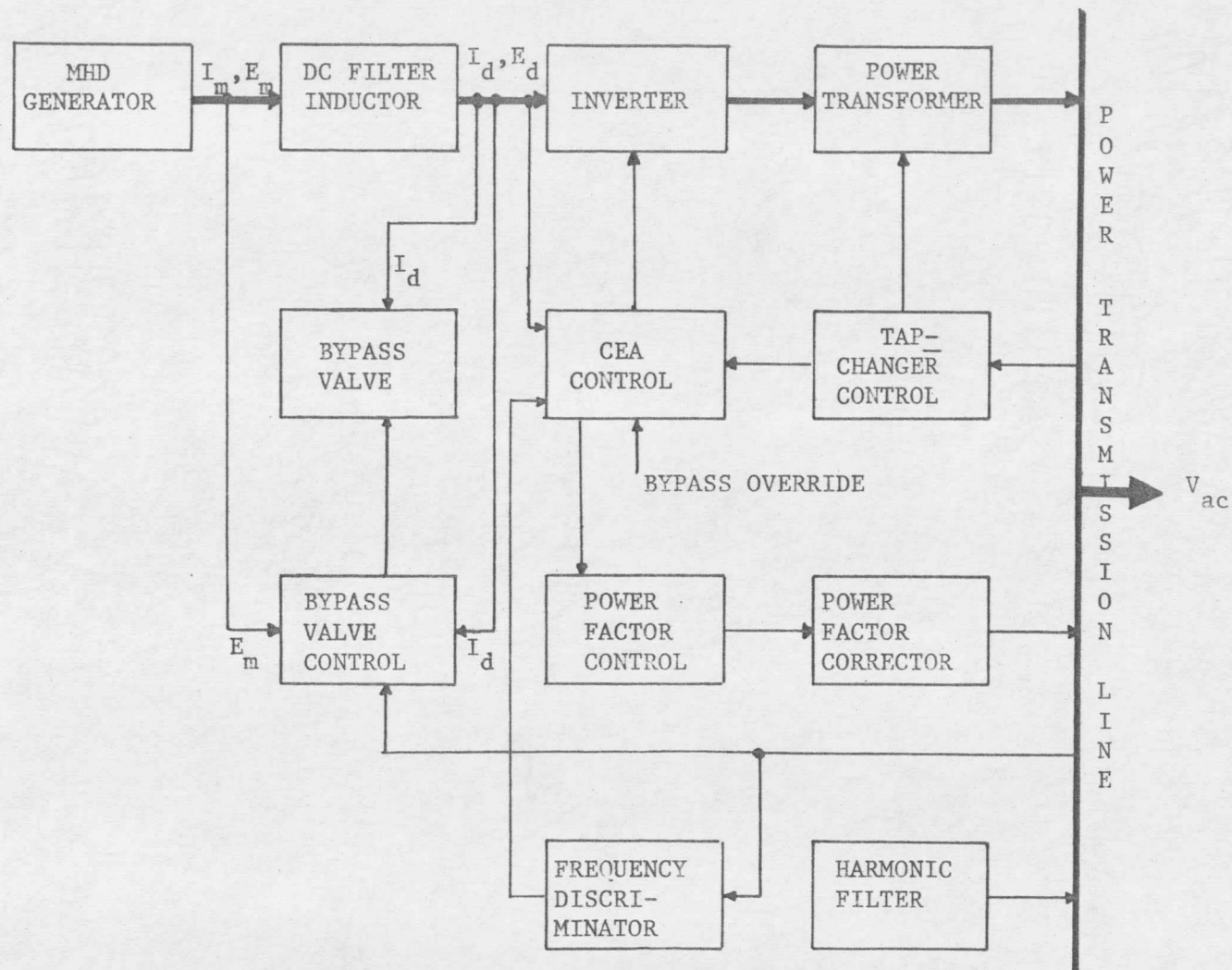


Figure 3.7. DC to AC Power Converter

DC filters may be necessary to smooth the input current of the inverter. An inductor is connected in series with the line for this purpose. The inductor also acts to reduce the harmonic voltages and currents in the DC line by smoothing the ripples.

3.3.1 The Inverter Circuit

The inverter circuit chosen for this study is the Graetz or the bridge type inverter (Figure 1.4) which is the most suitable inverter for high voltage DC operation [57-58,130]. The steady state analysis of this type of inverter is well documented [57-58,130-131]. Since the output of the inverter is connected to an existing AC transmission grid, phase-controlled commutation is employed.

To meet the system voltage requirement, series connection of the thyristors is necessary. In the series connected thyristors, unequal voltage distribution exists due to the differences in their leakage currents [138]. It is essential to maintain uniform voltage distribution under all conditions including external switching surges and atmospheric disturbances. This is achieved by connecting a voltage equalization (resistor) and damping circuits (resistor and capacitor in series) across each thyristor [135]. The capacitance compensates for the unequal recovery time of thyristors (see Figure 3.8).

The system current requirement can be met by parallel operation of thyristors. Matched thyristors (matched static and dynamic charac-

teristics) are used [136] to ensure equal current sharing and equal dissipation. However under different conditions the characteristics of matched thyristors may vary, thus some kind of external current sharing device must be used. One scheme is to use anode resistors (Figure 3.9) which can be adjusted to bring about closer current sharing. However this method would increase the voltage drop and the power loss in the whole circuit. For large power systems, current sharing transformers are more appropriate (Figure 3.10).

3.3.2 Formulation of the Dynamic Model

In the formulation of the dynamic model of the DC to AC power converter, the following assumptions will be made:

(1) Each section of the bridge inverter can be represented by a single thyristor with a finite forward resistance and an infinite backward resistance. The capacitance effects of the thyristors are neglected.

(2) The effects of the voltage and current equalization circuits are neglected.

(3) The inverter will be operated in an open-loop condition, thus control circuitry is not included in the model.

(4) The transformer link to the AC transmission grid will be represented by an equivalent resistance and inductance in series with an ideal source. Tap changing in the transformer is not included.

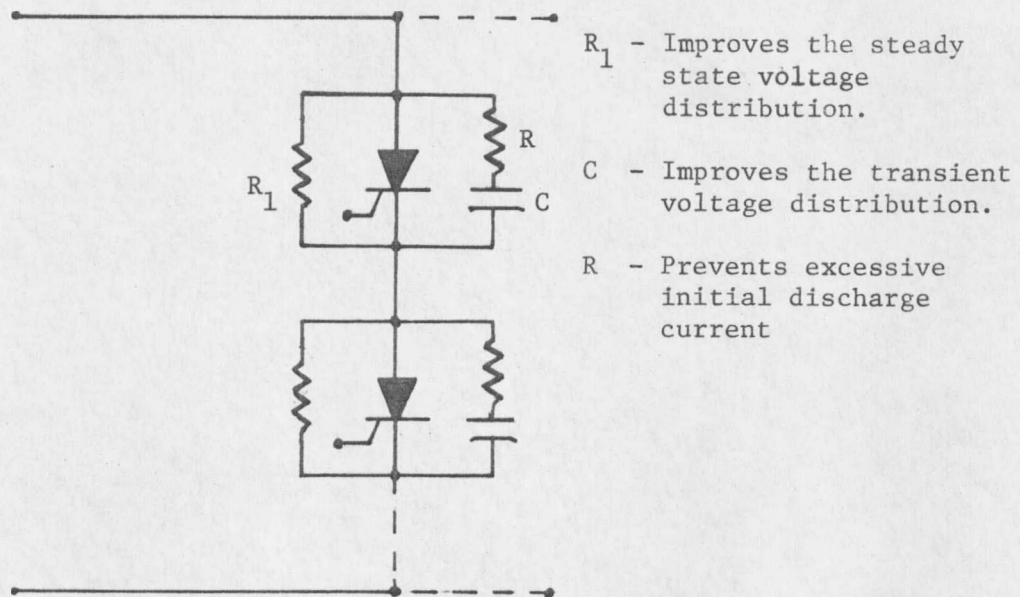


Figure 3.8. Voltage equalization circuit

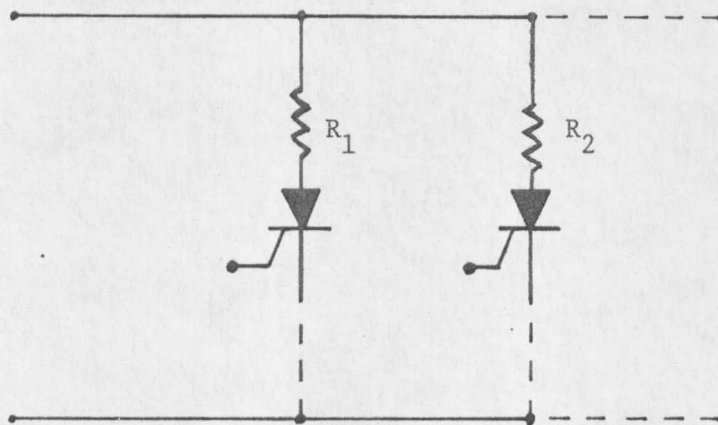


Figure 3.9. Current equalization circuit

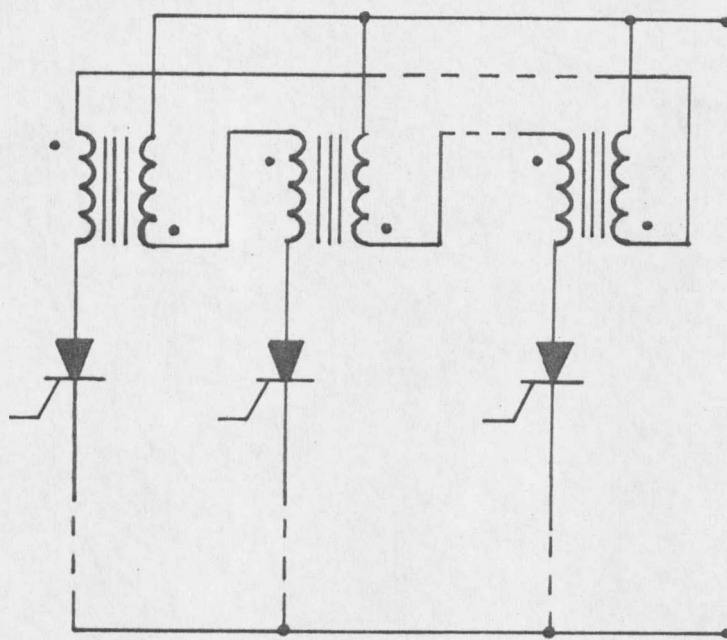


Figure 3.10. Current-sharing transformer

(5) A DC reactor is connected in series with the MHD generator in addition to the line resistance and line inductance.

(6) The effects of harmonics are neglected.

3.3.3 Inverter Operation

The inverter operation consists of consecutive processes, each process starts with the firing and commutation periods followed by a period in which two valves in series carry the full current. The process is completed by the firing of the next valve. At any given time the inverter is either in a commutating or non-commutating state.

To aid in the study of each process, each valve may be represented by the number of the process in which it fires ($1 + 6m$), where m is an integer ($m \geq 0$). Thus valve 1 will fire and conduct the direct current in processes 1, 7, 13, etc. The two consecutive processes (commutating and non-commutating) are illustrated in Figure 3.11.

A simplified equivalent circuit of the processes 1 and 2 may be obtained by removing the non-conducting valves and replacing the transformer by its equivalent circuit. The similarity between processes 1 and 2 (Figure 3.12a,b,c) makes it possible to represent each process by the single circuit of Figure 3.13. At the start of the commutation, the current (i) increases from zero until it is equal to current I_d at the end of the commutation state. When commutation is completed, the valve ($n - 2$) ceases to conduct, thus the current (i) is

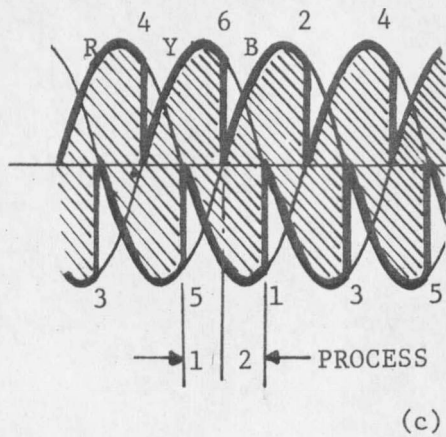
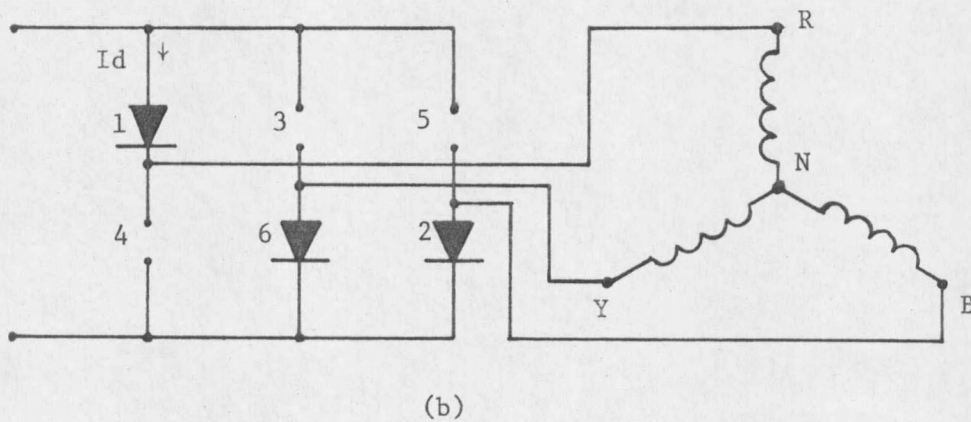
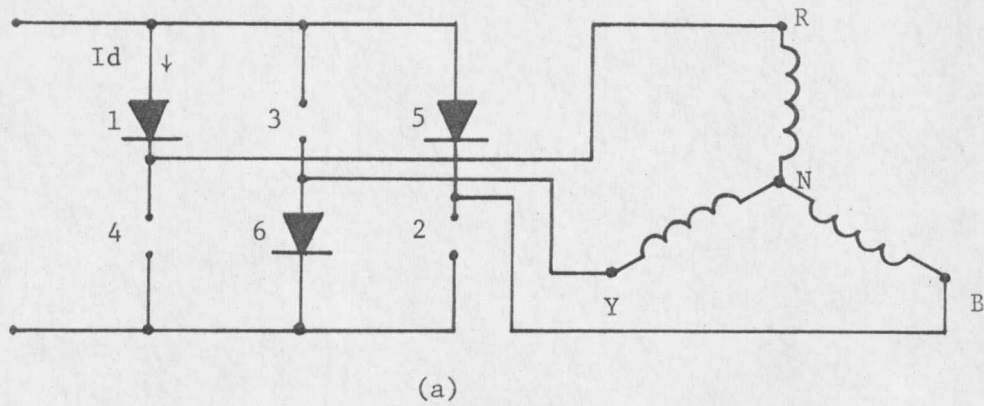
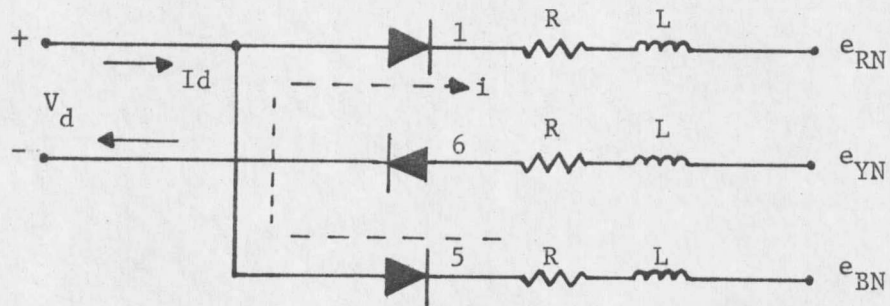
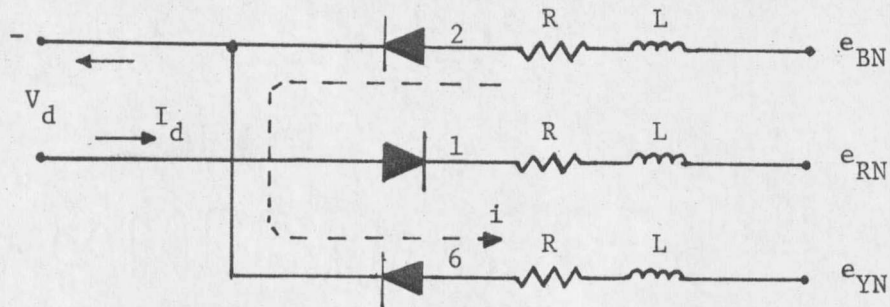


Figure 3.11.

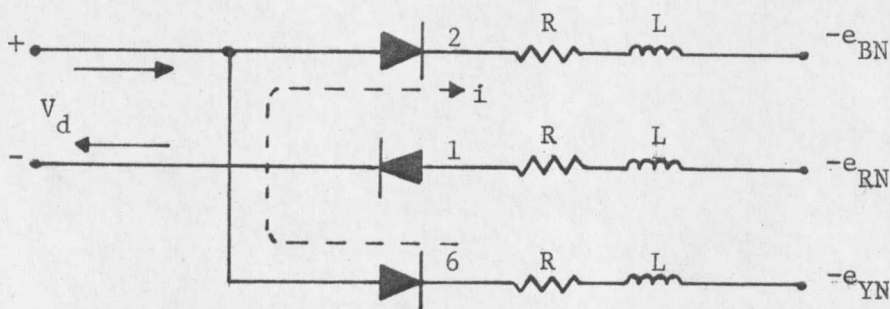
- a) Process 1: Commutating from valve 5 to valve 1.
- b) Process 2: Commutating from valve 6 to valve 2
- d) Voltage waveform



(a)



(b)



(c)

Figure 3.12. Equivalent Circuits of figure 3.11

- (a) Equivalent of figure 3.11a
- (b) Equivalent of figure 3.11b
- (c) Modification of (b) to the same form as (a)

equal to the current (I_d).

The time varying voltages e_{an} , e_{bn} , e_{cn} are different for each process. The current I_R , I_Y , and I_B are expressed in terms of the generator current (I_d) and the commutating current (i) (see Table 3.1). Since the transformer has a leakage inductance, the transfer of current from one phase to another requires a finite time (commutation time). The angle corresponding to the commutation time is referred to as the commutation or the overlap angle. The effect of the finite commutation time on the DC voltage waveshape is shown in Figure 3.14.

The two possible operating conditions of the inverter at any time are the commutating and the non-commutating states. The differential equations representing the behavior of the inverter may then be written (see Figure 3.13b):

Commutating State

From loop ABCFED

$$V_d - I_d(R_d + 2R) + e_{bn} - (e_{an} + e_{cn})/2 + IR + L \frac{di}{dt} - (L_d + 2L) \frac{dI_d}{dt} = 0 \quad (3.69)$$

From loop BCHGB

$$e_{an} - e_{cn} - 2iR + I_d R - 2L \frac{di}{dt} + L \frac{dI_d}{dt} = 0 \quad (3.70)$$

Table 3.1. Transformer alternating phase voltages corresponding to each process number and alternating line currents in terms of the direct current and the commutating current

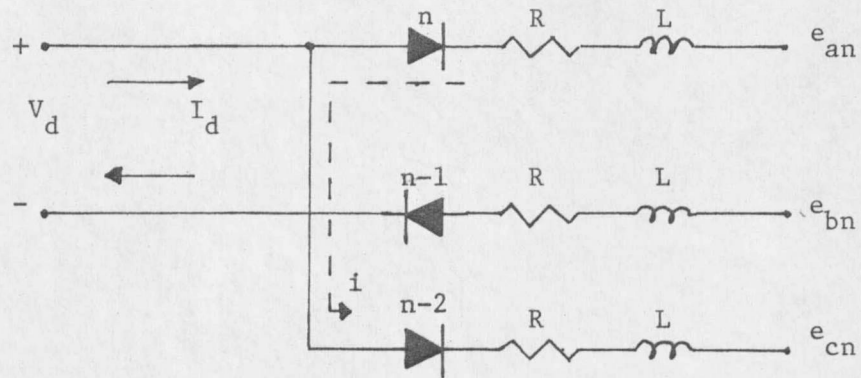
	1 + 6m	2 + 6m	3 + 6m	4 + 6m	5 + 6m	6 + 6m
e_{an}	e_{RN}	$-e_{BN}$	e_{YN}	$-e_{RN}$	e_{BN}	$-e_{YN}$
e_{bn}	e_{BN}	$-e_{YN}$	e_{RN}	$-e_{BN}$	e_{YN}	$-e_{RN}$
e_{cn}	e_{YN}	$-e_{RN}$	e_{BN}	$-e_{YN}$	e_{RN}	$-e_{BN}$
I_R	i	I_d	$I_d - i$	$-i$	$-I_d$	$-(I_d - i)$
I_Y	$-I_d$	$-(I_d - i)$	i	I_d	$I_d - i$	$-i$
I_B	$I_d - i$	$-i$	$-I_d$	$-(I_d - i)$	i	I_d

Non-commutating State

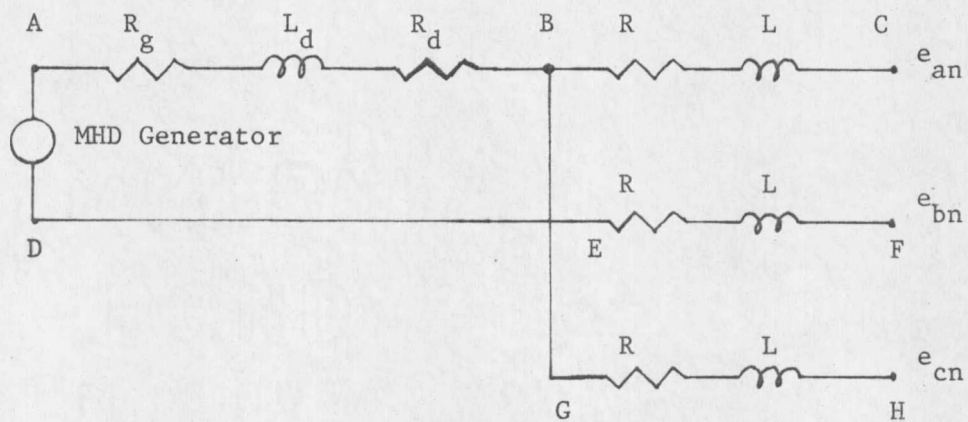
$$\begin{aligned}
 V_d + e_{bn} - e_{cn} + iR - I_d(R_d + 2R) \\
 + L \frac{di}{dt} - (L_d + 2L) \frac{dI_d}{dt} = 0
 \end{aligned} \tag{3.71}$$

$$\frac{di}{dt} = \frac{dI_d}{dt} \tag{3.72}$$

$$i = I_d \tag{3.73}$$



(a) Equivalent circuit



(b) Generalized circuit

Figure 3.13. Inverter generalized equivalent circuit.

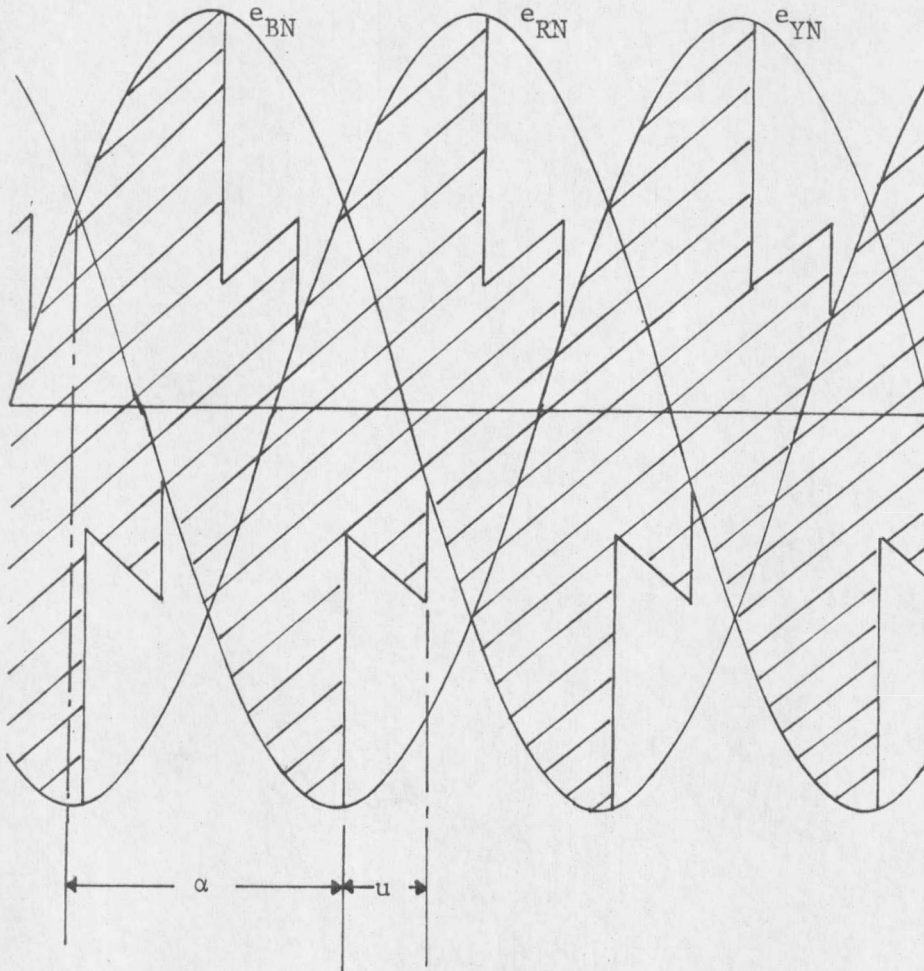


Figure 3.14. Inverter Voltage Waveshape

α - delay angle
 u - overlap angle

Chapter IV

COMPUTER IMPLEMENTATION OF THE SYSTEM MATHEMATICAL MODEL

4.1 The MHD Generator and Diffuser

The behavior of the MHD generator and diffuser is characterized by a set of partial differential equations and a set of algebraic equations defined in chapter II. The motion of the fluid in the MHD channel is often characterized by discontinuities of the fluid parameters or its derivatives even if the flow is smooth in the beginning [141]. The different types of discontinuities are contact discontinuity (mass density and energy are discontinuous but pressure and velocity are continuous), a shock (where mass density, energy, pressure and velocity are all discontinuous), and a rarefaction (only certain derivatives are discontinuous).

Several methods are available to solve the time-dependent MHD equations in which shock may occur. A method which is applicable only to shock-free MHD flow may be used in conjunction with shock-patching methods. The shock is maintained as a discontinuity and the Rankine-Hugoniot relations [144] are applied across the discontinuity. This is applicable only in one-dimensional problems utilizing the Eulerian variables [75]. Another method involves the changing of the numerical procedures to continue the integration through the discontinuity. The procedure may be a polynomial curve-fitting technique and a fine mesh

near the shock [145]. Several shock patching methods are described in references [75,143].

A most suitable method would be one that allows the formation of a shock in the MHD flow and also allows the numerical calculation to proceed without using any special treatment of the shock or even requiring the detection of the shock. The most widely used method is the Lax-Wendroff method [146-147] and its modified forms [75,141]. Several modifications of the original Lax-Wendroff method are also discussed in reference [143].

In this study, the MHD equations were solved by employing the two-step Lax Wendroff method. This method based on the system of conservation equations (mass density, momentum density and energy density as dependent variables) enables one to integrate across the discontinuity. If the method is applied to non-conservative equations (mass density, velocity and temperature as dependent variables), significant errors will occur in crossing the shock wave [147]. However, an accurate solution may be obtained over the region outside the shock and even in the presence of a rarefaction wave.

4.11 Derivation of the Conservation Equations

The MHD equations are (from section 2.3)

$$\frac{\partial \rho}{\partial t} + \frac{1}{A} \frac{\partial}{\partial x}(\rho u A) = 0 \quad (2.27)$$

$$\rho \frac{\partial u}{\partial t} + \rho u \frac{\partial u}{\partial x} = - \frac{\partial p}{\partial x} + (JB)_x - F \quad (2.28)$$

$$\rho \frac{\partial h}{\partial t} + \rho u \frac{\partial h}{\partial x} + \rho u \frac{\partial u}{\partial x} = \frac{\partial p}{\partial t} + \quad (2.29)$$

$$JE - G \quad (2.29)$$

The above equations have to be converted to the conservation law form, in which the dependent variables are the mass density, the momentum density and the internal stagnation energy density. The conservation equations are:

Conservation of Mass density

$$\frac{\partial \rho}{\partial t} + \frac{1}{A} \frac{\partial}{\partial x}(\rho u A) = 0 \quad (4.1)$$

Conservation of Momentum density

$$\begin{aligned} \frac{\partial m}{\partial t} + \frac{\partial}{\partial x}(m^2/\rho + p) + F - (JB)_x + \\ (m^2/\rho A) \frac{dA}{dx} = 0 \end{aligned} \quad (4.2)$$

Conservation of Stagnation Internal Energy density

$$\begin{aligned} \frac{\partial}{\partial t} E_s + \frac{\partial}{\partial x}[m(E_s + p)/\rho] + G - JE \\ + [m(E_s + p)/\rho A] \frac{dA}{dx} = 0 \end{aligned} \quad (4.3)$$

where $m = \rho u$

$$E_s = \rho (e + u^2/2)$$

$$e = h - p/\rho$$

The conservation equations are of the form

$$\frac{\partial U}{\partial t} + \frac{\partial F(U)}{\partial x} + G(U) = 0 \quad (4.4)$$

The two-step Lax Wendroff method presented in reference [75] is for fluids which are non-viscous and thermally non-conducting characterized by the form

$$\frac{\partial U}{\partial t} + \frac{\partial F}{\partial x} = 0 \quad (4.5)$$

Since the MHD equations include viscous friction, heat loss to the walls and power extraction, a modified two-step Lax Wendroff method will be used. In this method the values of U (the dependent variables defined in equation 4.4) are computed for temporary points at half-time and half-space increments, $(n + 1/2, j \pm 1/2)$ in the first step of calculation. The second step is then employed to determine the value of U at the final point $(n + 1, j)$. The finite difference calculation procedure for equation 4.4 is as follows:

Step One

$$\begin{aligned}
 U_{j+1/2}^{n+1/2} &= (U_{j+1}^n + U_j^n)/2 - \frac{\Delta t}{\Delta x} (F_{j+1}^n - F_j^n) \\
 &\quad - \frac{\Delta t}{2} (G_{j+1}^n + G_j^n)
 \end{aligned} \tag{4.5}$$

$$\begin{aligned}
 U_{j-1/2}^{n+1/2} &= (U_{j-1}^n + U_j^n)/2 - \frac{t}{x} (F_{j-1}^n - F_j^n) \\
 &\quad - \frac{t}{2} (G_{j-1}^n + G_j^n)
 \end{aligned} \tag{4.6}$$

Step Two

$$\begin{aligned}
 U_j^{n+1} &= U_j^n - \frac{\Delta \Delta t}{\Delta x} (F_{j+1/2}^{n+1/2} - F_{j-1/2}^{n+1/2}) \\
 &\quad - \frac{\Delta t}{2} (G_{j+1/2}^{n+1/2} - G_{j-1/2}^{n+1/2})
 \end{aligned} \tag{4.7}$$

The stability limitation is given by the Courant, Freidrick and Lewy stability criterion [142]

$$(|u| + a) \frac{\Delta t}{\Delta x} \leq 1 \tag{4.8}$$

which means that the sound wave cannot travel more than one cell length in one time increment. This stability condition is often comparable to an elaborate matrix stability analysis [143].

4.2 The Steam Bottoming Plant and the Air Heater

The behavior of the steam plant and the air heaters are characterized by a set of ordinary differential equations of the form

$$\frac{dU}{dt} + F(U) = 0 \quad (4.9)$$

and a set of algebraic equations. There are several numerical methods that can be used in solving a system of first order differential equations. The choice of the method to be used is based on many factors such as:

(1) The accuracy required. The error in the solution depends on the error incurred at each step of the integration and the propagation of the error in later steps. (2) The speed of which the computation will be performed. (3) The ease with which the estimation of the errors at each step may be made. (4) The ease with which a method can be programmed for a computer solution and the availability of the computer program.

The various numerical procedures are roughly classified into two groups, the one-step method and the multi-step method [148], employing either the direct or indirect use of Taylor's expansion series of the solution function. The single step method uses only the results obtained for (t_i) in the approximation of the value at (t_{i+1}) . In the multi-step method the results obtained for

($t_i, t_{i-1}, t_{i-2}, \dots, t_{i-p}$) are required, the number p being dependent on the formula utilized. There are many methods in both categories [148-150] but the most commonly used methods are the Runge-Kutta (single step) and the Predictor-corrector (multi-step) methods. [151].

The characteristics of these two methods are:

(a) Runge-Kutta method

(1) They are self-starting, (2) they require several evaluations of the function and are therefore time consuming (a fourth order requires four evaluations of the function), (3) being self-starting, they permit an easy change in the step size, (4) they provide no easily obtainable information about the truncation error.

(b) Predictor-corrector method

(1) They are not self-starting hence requires a method such as the Runge-Kutta method for starting, (2) since they require information about prior points for repeated evaluation of the function, they are more efficient, (3) a change in the step size requires a temporary reversion to a Runge-Kutta method, (4) a good estimate of the truncation error is available out of the computational algorithm.

A more complete and useful method would be a combination of the Runge-Kutta (to start the computation) and the predictor-corrector method (to evaluate succeeding values of the function). However,

the complexity of the program involving both methods makes it imperative to use the single-step method. Furthermore, the Runge-Kutta method is comparable in accuracy, often more accurate than corresponding order predictor-corrector method although it requires twice as much calculation time. Because of the difficulty of estimating the per step error, the step size may be chosen conservatively (smaller than is actually necessary) to achieve the desired accuracy.

A fourth order Runge-Kutta algorithm was used with the step size varied according to the accuracy required. A derivation of this algorithm may be found in reference [148-150].

The set of ordinary first order differential equations is expressed in explicit form

$$\frac{dy}{dt} = f(y,t) \quad (4.10)$$

The fourth order Runge-Kutta calculation procedures is

$$y_{i+1} = y_i + \frac{h}{6} (k_1 + 2k_2 + 2k_3 + k_4) \quad (4.11)$$

where $k_1 = f(t_i, y_i)$

$$k_2 = f\left(t_i + \frac{h}{2}, y_i + \frac{h}{2} k_1\right)$$

$$k_3 = f\left(t_i + \frac{h}{2}, y_i + \frac{h}{2} k_2\right)$$

$$k_4 = f(t_i + h, y_i + hk_3)$$

where h is the step size.

Since the error will not be calculated in the Runge-Kutta method, the differential equations are solved using different step sizes and the behavior of the solution is observed with regards to stability and convergence. To reduce the round-off errors, double precision arithmetic is employed.

4.3 The DC to AC Power Converter

The mathematical model representing the DC to AC power conversion circuit consists of differential, algebraic and boolean equations (equations 3.69 - 3.73). The two sets of differential equations represent two different states of operation, namely the commutating and the non-commutating states. The discontinuity occurs by virtue of the valve changing its conducting state.

The computer flow diagram employed in solving the set of differential equations with discontinuities is shown in Figure 4.1. The differential equations are solved using the fourth order Runge-Kutta method (similar to that used for the steam plant). This method is suitable for equations which contain discontinuities because each step is computed independently. Computation is done up to the point of discontinuity and then restarted from the disconti-

nulty with appropriate changes in the system differential equations.

4.4 Computer Program to Simulate the Mathematical Model

The computer program used to simulate the quasi-one dimensional MHD equations is shown in Appendix G. The author used the computer program (Runge-Kutta algorithm) developed by Dr. R. M. Johnson to simulate the behavior of the steam plant the air heater and the DC to AC power converter. This program is on file in the Computer Center system library.

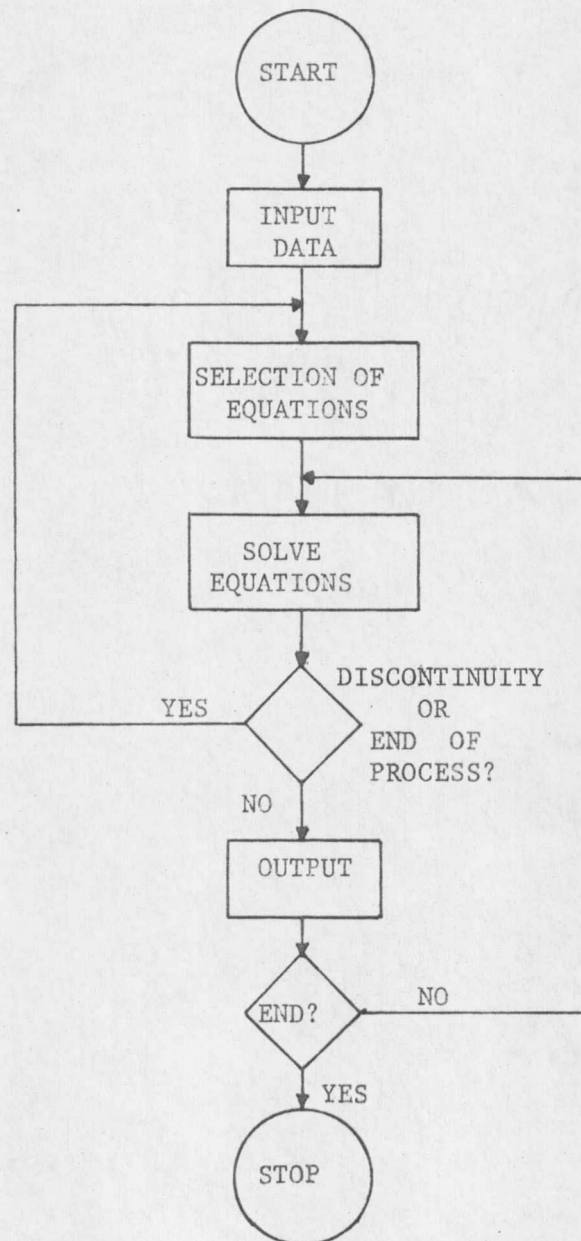


Figure 4.1. Computer Program Flow Diagram - DC to AC Power Conversion Circuit

Chapter V

RESULTS OF THE COMPUTER SIMULATION

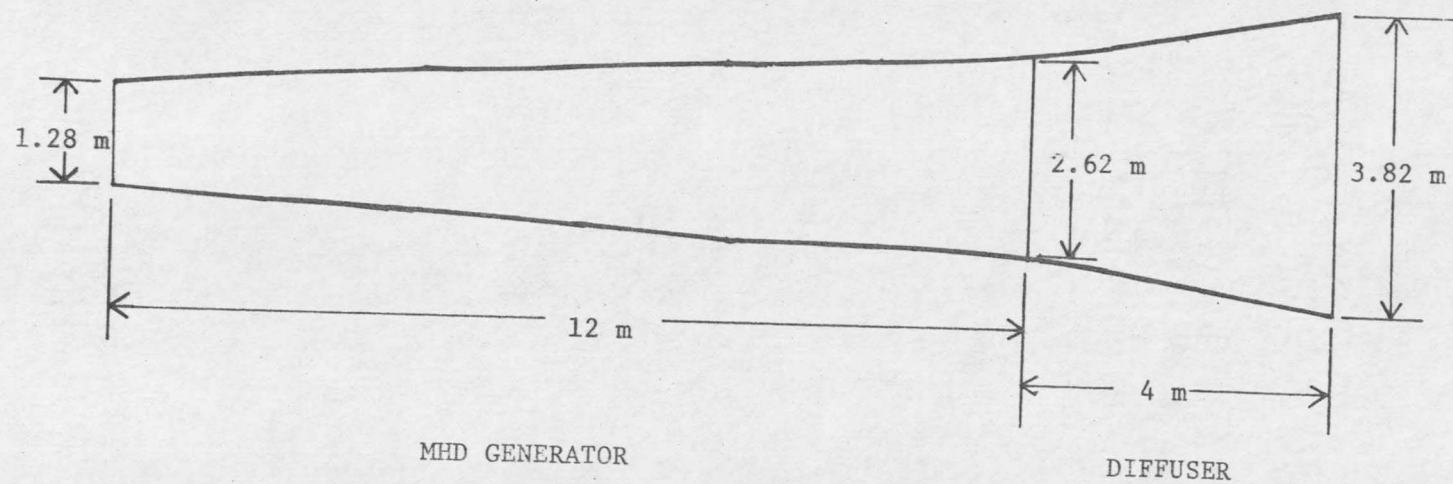
5.0 Introduction

The mathematical model of the MHD system components were implemented by the use of the Fortran IV programming language. All the arithmetic operations were performed in double precision to minimize truncation errors. The results of the simulation of the time-dependent flow in the MHD channel and the simulation of the dynamic behavior of the steam bottoming plant are presented in this chapter.

5.1 Steady State Flow in the MHD Generator

A magnetohydrodynamic generator and diffuser was designed using the non-conservative, quasi one-dimensional steady state MHD equations. The flow section of the MHD channel is shown in Figure 5.1. The MHD generator is segmented and diagonally connected with a single external load resistance. There is no regulation system connected to the external circuit. However, the load resistance may be varied to obtain the transient response of the generator. The MHD generator design parameters are shown in Table 5.1.

The steady state performance of the MHD generator under three different load current conditions were obtained and the results are shown in Figures 5.2 to 5.6. The voltage-current characteristic of the generator is shown in Figure 5.2.



108

Figure 5.1. Diagram of the flow section of the channel

Table 5.1. MHD Generator Design Parameters

(1) Geometry

Electrode pitch	-	4 cm
Electrode width	-	3 cm
Insulator (gap) width	-	1 cm
Cross-connection parameter	-	1.28
Channel cross-section, Inlet	-	1.644 m
Exit	-	6.859 m
Channel length	-	12 m
No. of Electrodes	-	300

(2) Gas Dynamic Variables

Pressure, Inlet	-	3.98 atmospheres
Exit	-	.987 atmospheres
Gas Temperature, Inlet	-	2590° Kelvin
Exit	-	2300° Kelvin
Gas Velocity, Inlet	-	765 meters/sec
Exit	-	625 meters/sec
Mach number, Inlet	-	.776
Exit	-	.672
Wall Temperature	-	1300° Kelvin

Table 5.1. MHD Generator Design Parameters (continuation)

(3) Combustion conditions

Working Fluid - Combustion products of sub-bituminous coal,
oxygen enriched air and potassium seed.

Seed - .5 % potassium (mole fraction)

Combustion temperature - 2700° Kelvin

Combustion pressure - 6 atmospheres

Air pre-heat temperature - 1480° Kelvin

Mass flow rate , air - 580 Kg/sec

coal - 72.8 Kg/sec

oxygen - 17.3 Kg/sec

seed - 7.3 Kg/sec

(4) Electrical parameters

Electrical conductivity, Inlet - 6.1 mho/meter

Exit - 3.34 mho/meter

Magnetic field - 6 Tesla

Load current - 16000 amperes

Terminal voltage - 29,570 volts

Open-circuit Voltage - 65,625 volts

Maximum current density, J_y - 10800 amperes/sq m

J_x - 4700 amperes/sq m

Maximum Hall field - 2870 volts/meter

Voltage drop parameter - .007

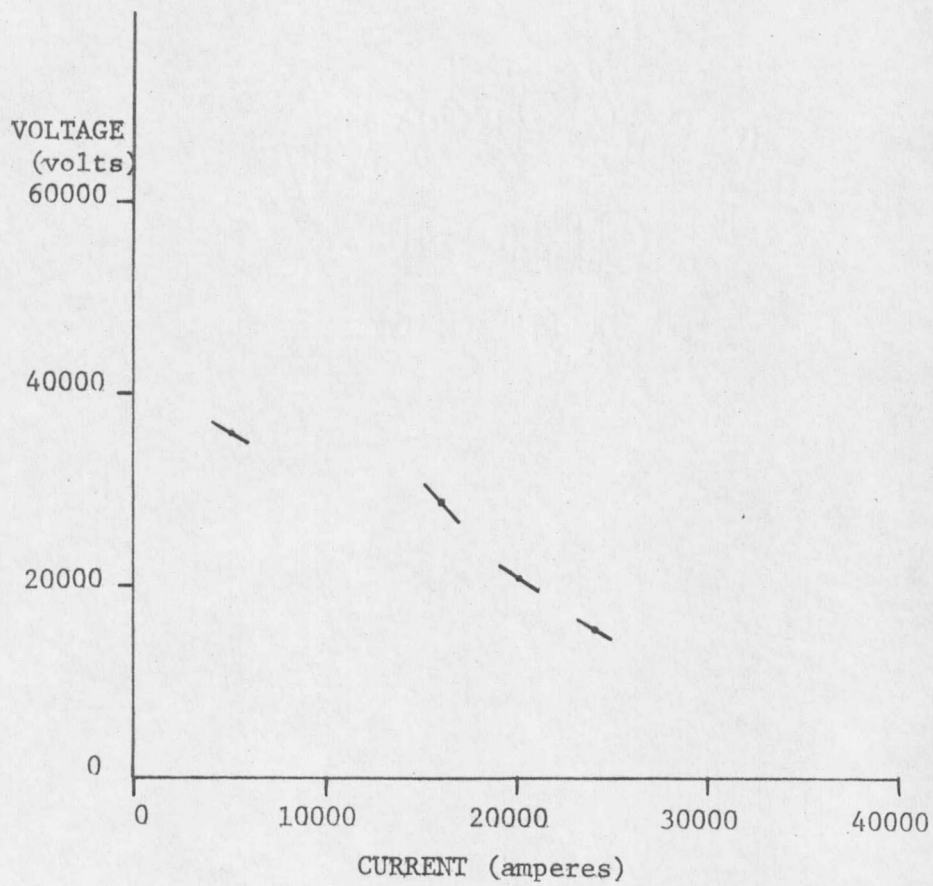


Figure 5.2. Voltage-current characteristic of the MHD Generator

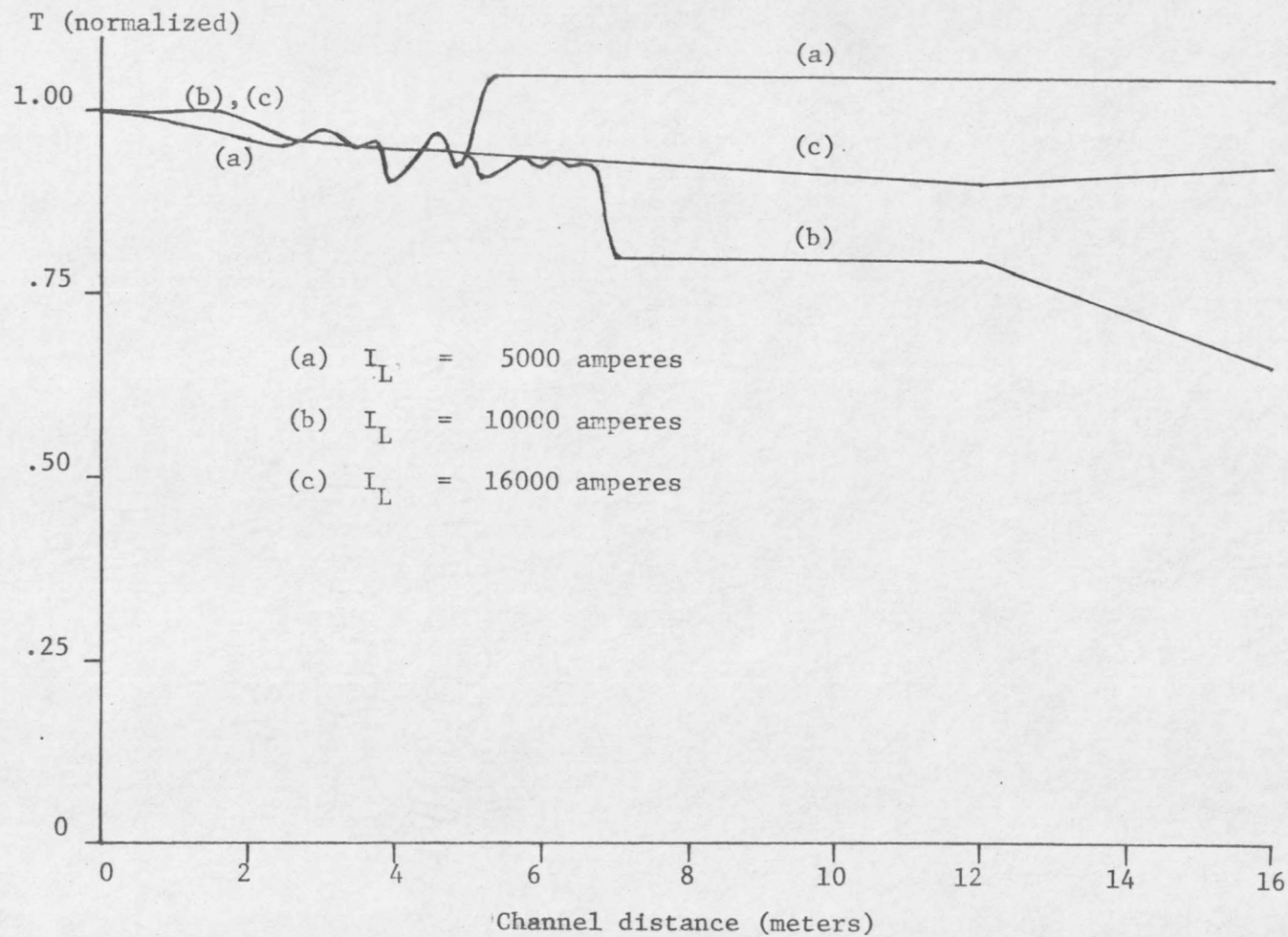


Figure 5.3. Variation of Temperature (normalized) with channel distance for three different load current conditions.

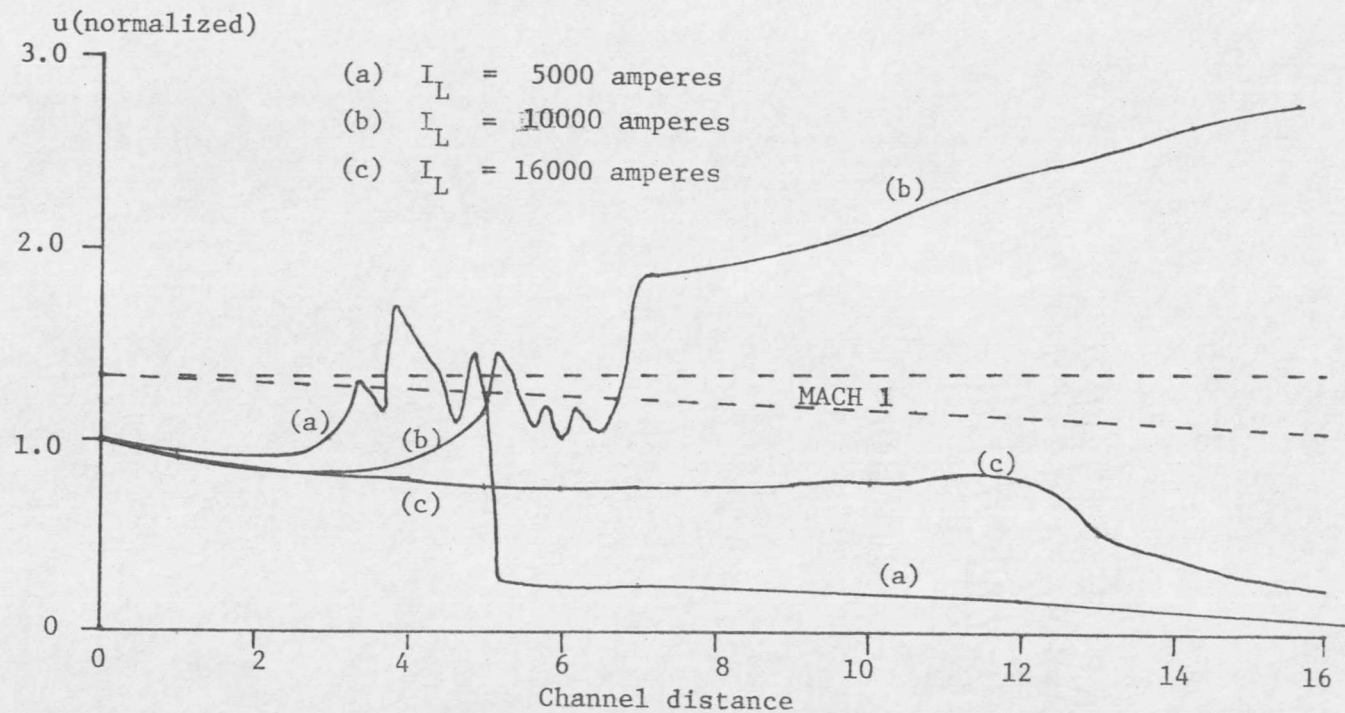


Figure 5.4. Variation of Gas Velocity with Channel distance for three different load current conditions.

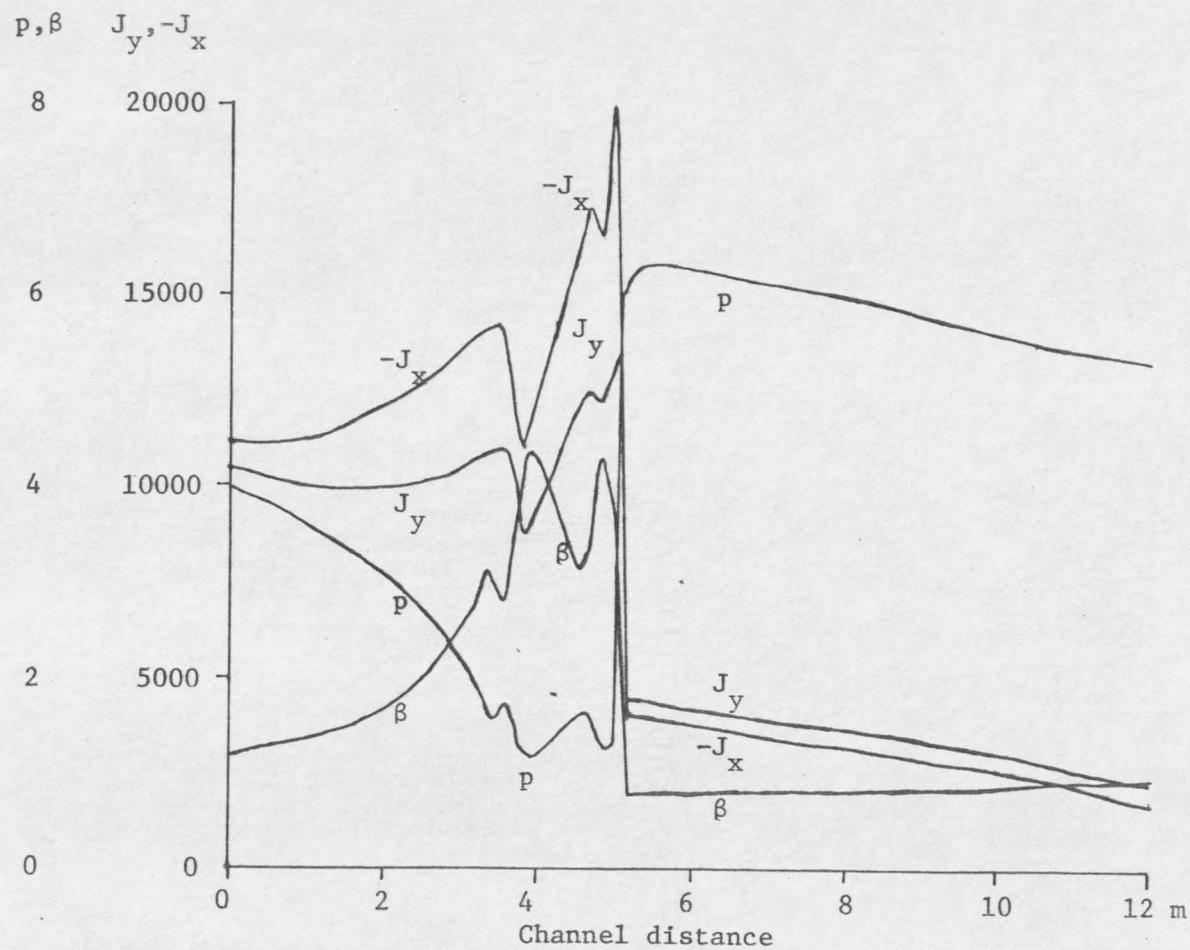


Figure 5.5. Variation of pressure (atmosphere), current density (amperes per sq meter), and Hall parameter with channel distance.
Load current = 5000 amperes

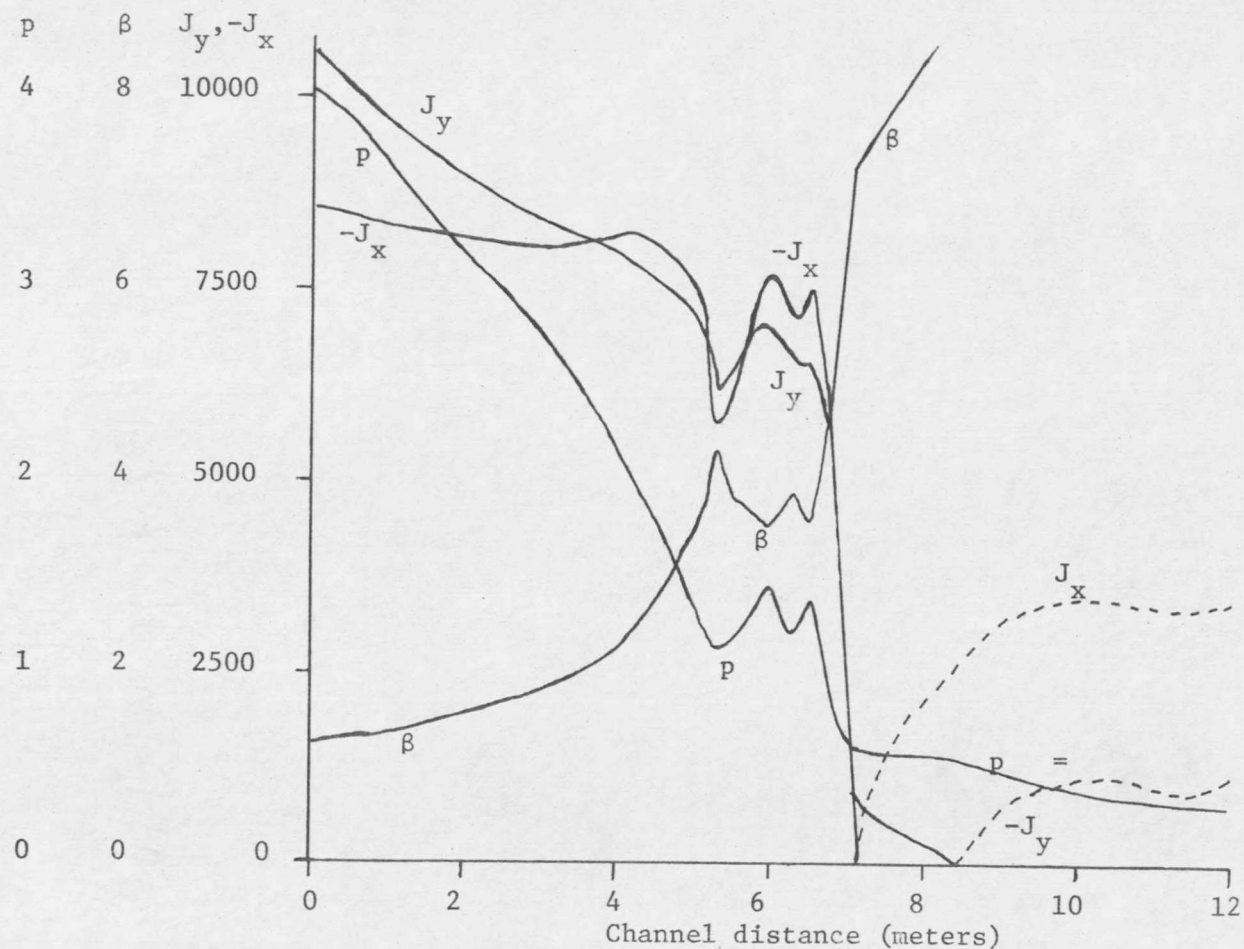


Figure 5.6. Variation of pressure (atmosphere), current density (amperes per sq meter) and Hall parameter with channel distance.
Load current = 10000 amperes.

At a load current of 5000 amperes, a compression shock occurred at approximately five meters ($x = 5 \text{ m}$) from the generator inlet. The result was a sudden increase in the gas pressure and temperature and a decrease in the gas velocity. The power output dropped to 38 % of the designed power output (473 MW at 16000 amperes).

At a load current of 10000 amperes, power is generated for the first 7.4 meters of the channel. Beyond 7.4 meters, the gas flow is accelerated. The gas velocity exceeded Mach one (1) at the generator exit, thus further acceleration of the gas flow occurred in the subsonic diffuser.

5.2 Time-dependent Flow in the MHD Channel

The behavior of the MHD generator and diffuser was simulated using the model described in chapter II. It was assumed that the gas enters the MHD channel from a large reservoir and is discharged from the diffuser into a large volume (such as the radiant section of the steam plant). Thus, the initial and boundary conditions (the stagnation parameters) may be considered fixed.

The MHD equations are in conservation form (equations 4.1 to 4.3)

$$\frac{\partial U}{\partial t} + \frac{\partial F(U)}{\partial x} + G(U) = 0 \quad (5.1)$$

An initial condition ($t = 0$) is assumed, such that a steady

state flow exists with a known distribution of the gas dynamic, thermodynamic and electrical parameters:

$$\rho = \rho(x); \quad m = m(x); \quad E_s = E_s(x) \quad (5.2)$$

$$F = F(x); \quad G = G(x) \quad (5.3)$$

$$J_x = J_x(x); \quad J_y = J_y(x) \quad (5.4a)$$

$$E_x = E_x(x); \quad E_y = E_y(x) \quad (5.4b)$$

The distribution was obtained from the numerical solution of a system of differential equations describing the steady state flow of the working fluid in the MHD channel. This set of parameters was then introduced as initial conditions for the system of conservative time-dependent MHD flow equations. The MHD time-dependent flow equations were simulated for seven (7) milliseconds and the resulting gasdynamic parameters (Figure 5.7 to 5.9) were used as initial conditions for the succeeding calculations.

The initial and boundary conditions for the system of equations (equations 4.1 to 4.3) are

$$p_o = p_{1o} = \text{constant, if } x = 0 \quad (5.5)$$

$$E_{so} = E_{s1o} = \text{constant, if } x = 0 \quad (5.6)$$

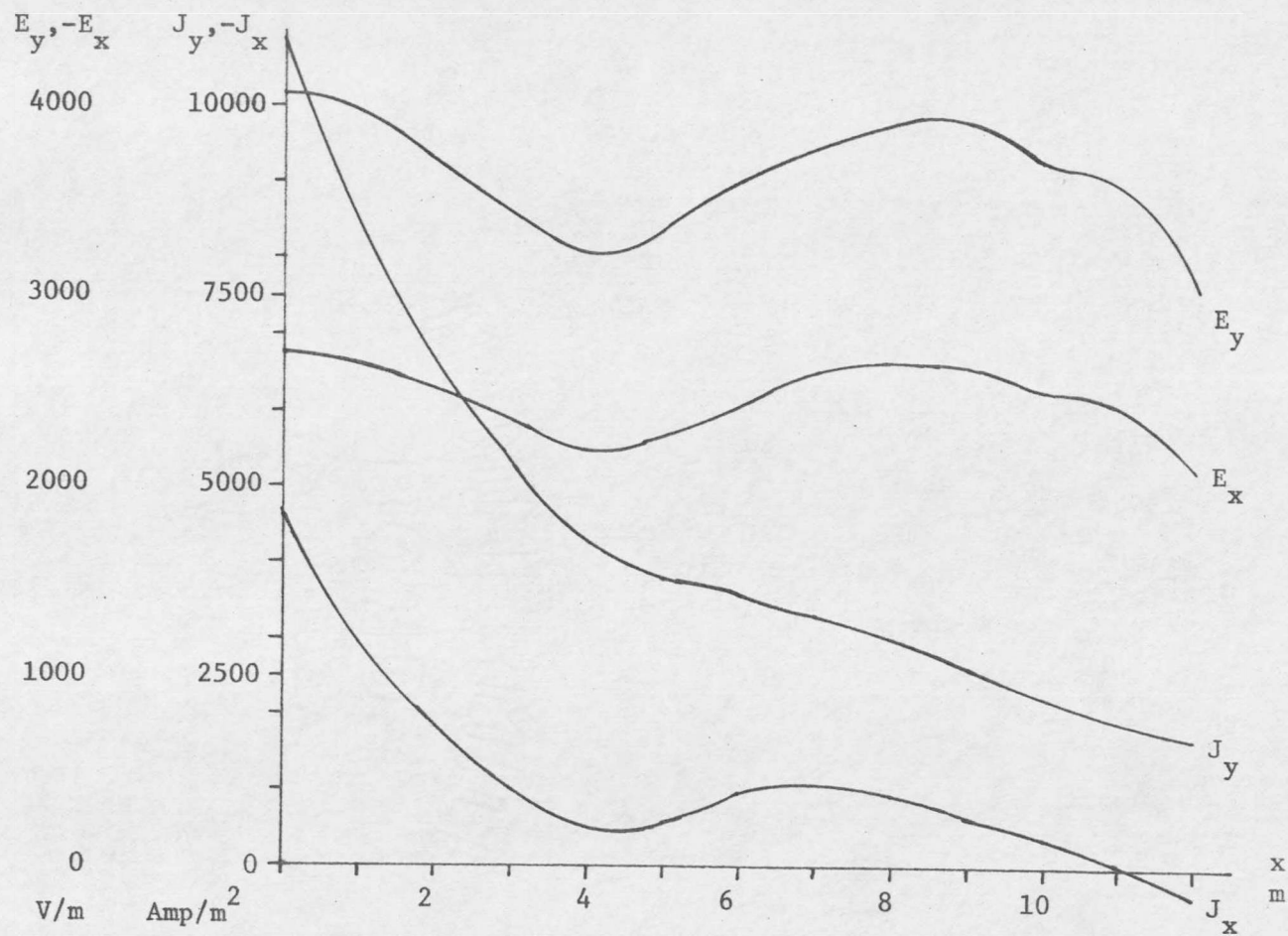


Figure 5.7. Steady State Electrical Parameters

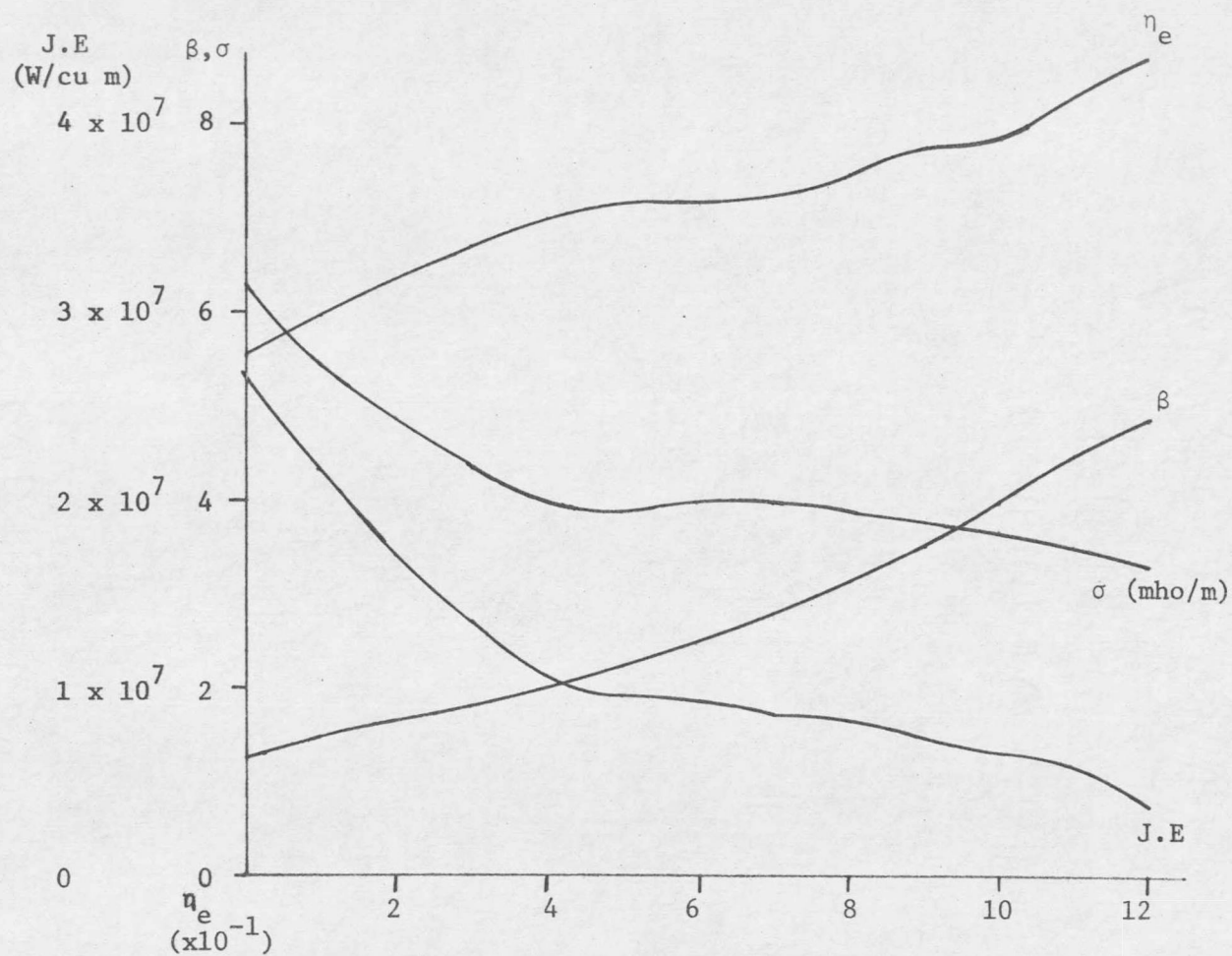


Figure 5.8. Steady State Electrical Parameters

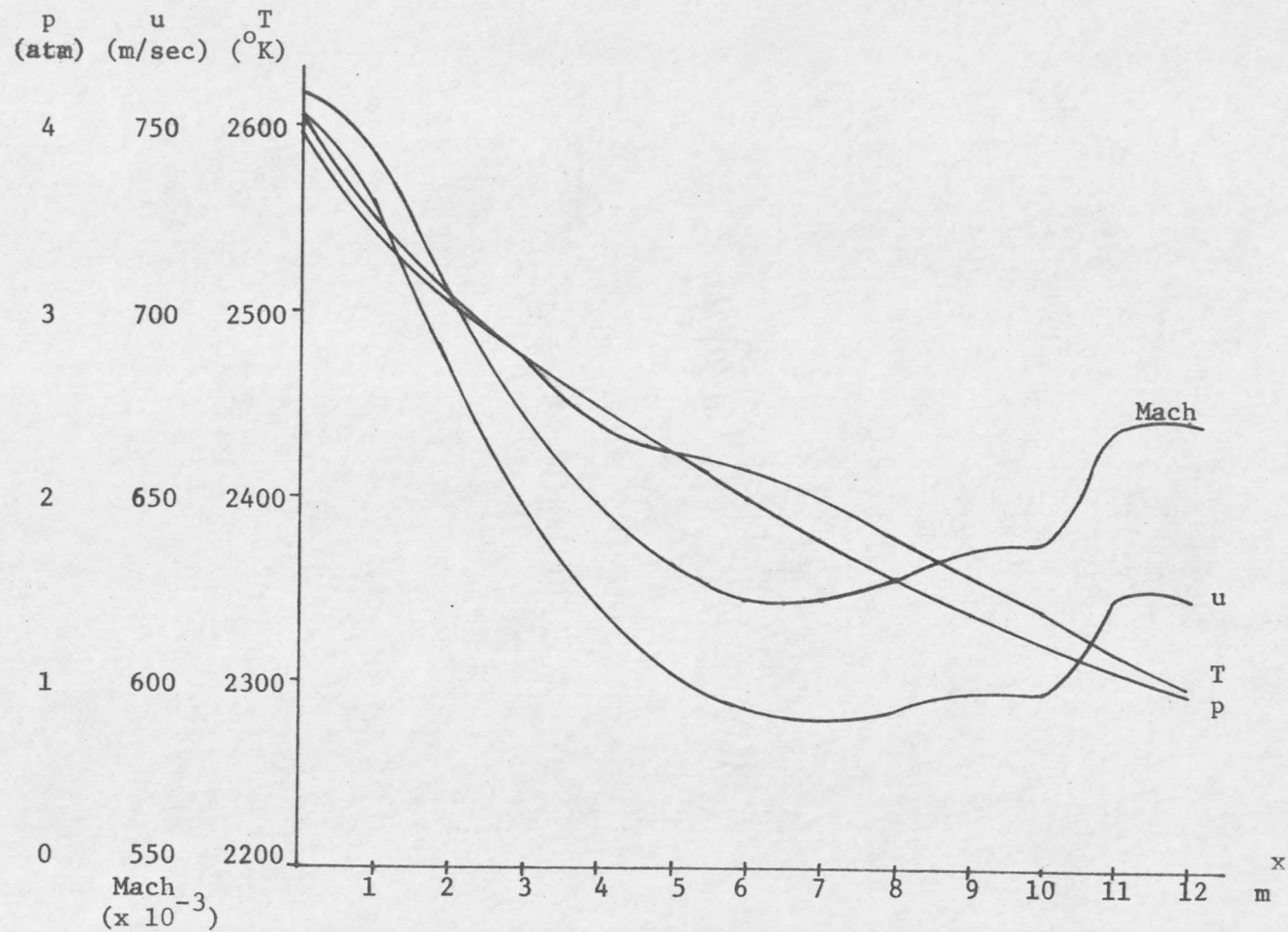


Figure 5.9. Steady State Gas Dynamic Parameters

$$p = p_2 = \text{constant, if } x = 16 \text{ meters} \quad (5.7)$$

where p_{10} , E_{s10} , and p_2 are the corresponding stagnation parameters of the working fluid.

5.2.1 Transient Response of the MHD Generator

A pure resistance was connected across the terminals of the MHD generator such that the operating current is equal to 16000 amperes. The transient response of the generator was obtained by changing the load resistance (instantaneously) to approximately 52 % of the steady state load resistance. The response of the generator to this step change in the load resistance is shown in Figures 5.10 to 5.12. In Figure 5.10 the variation of the terminal voltage and load current (normalized with the values at $t = 0$), and the generator internal resistance with time are plotted. The terminal voltage and the load current reached their steady state values in approximately 16 milliseconds. However, the generator internal resistance required more time to stabilize (approximately 24 milliseconds). The variations of the generator electrical and gasdynamic parameters (at $t = 23.2$ milliseconds) are shown in Figures 5.11 to 5.12. The increase (+ 52 %) in the load resistance caused the generator terminal voltage to increase (+ 18.9 %), the load current to decrease (-22%) and the power output to decrease (7.23 %). A portion of the internal energy of the working

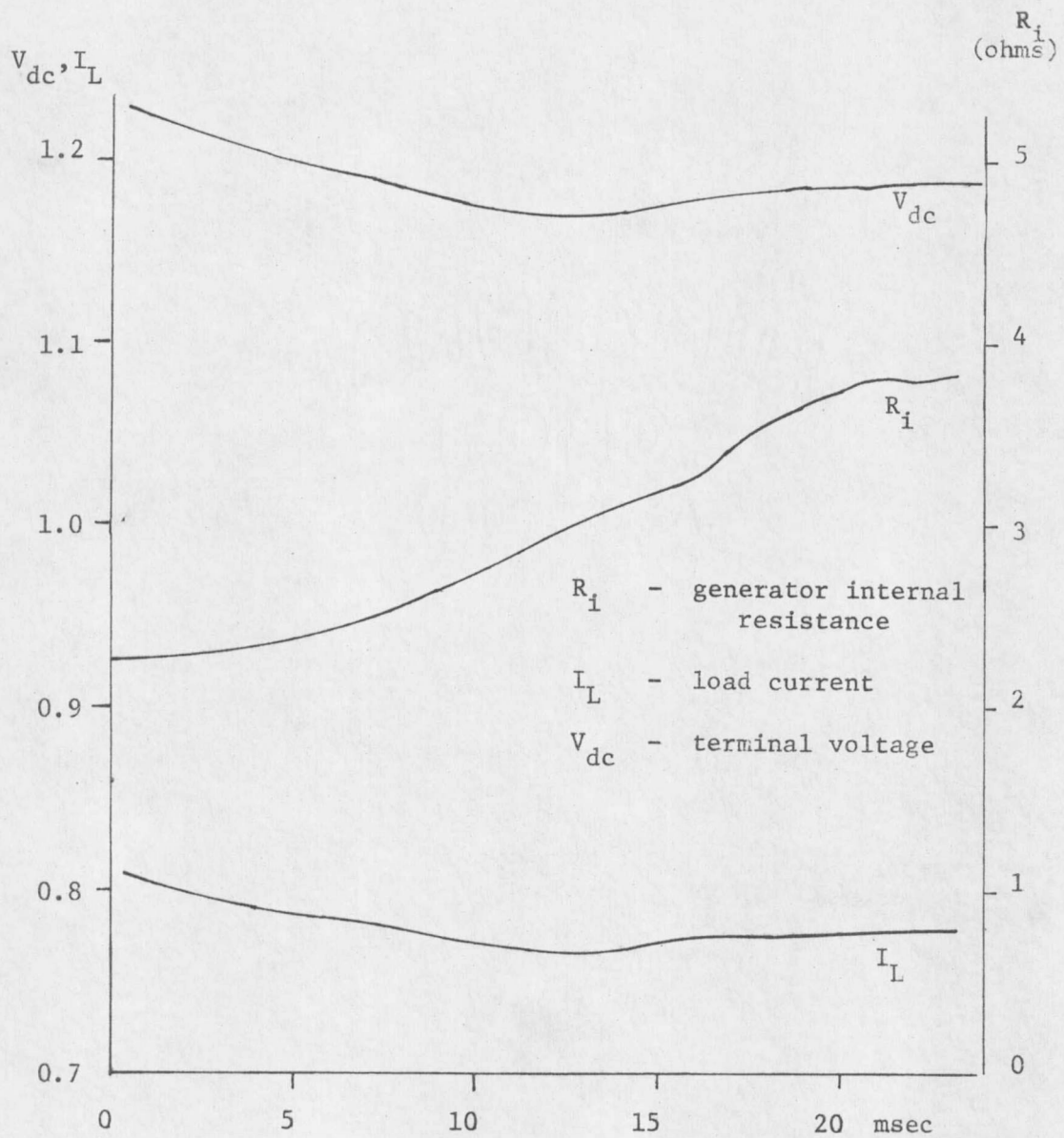


Figure 5.10a. Response of the MHD Generator to a + 52 % step change in the load resistance (V_{dc} and I_L are normalized with the values at $t = 0$).

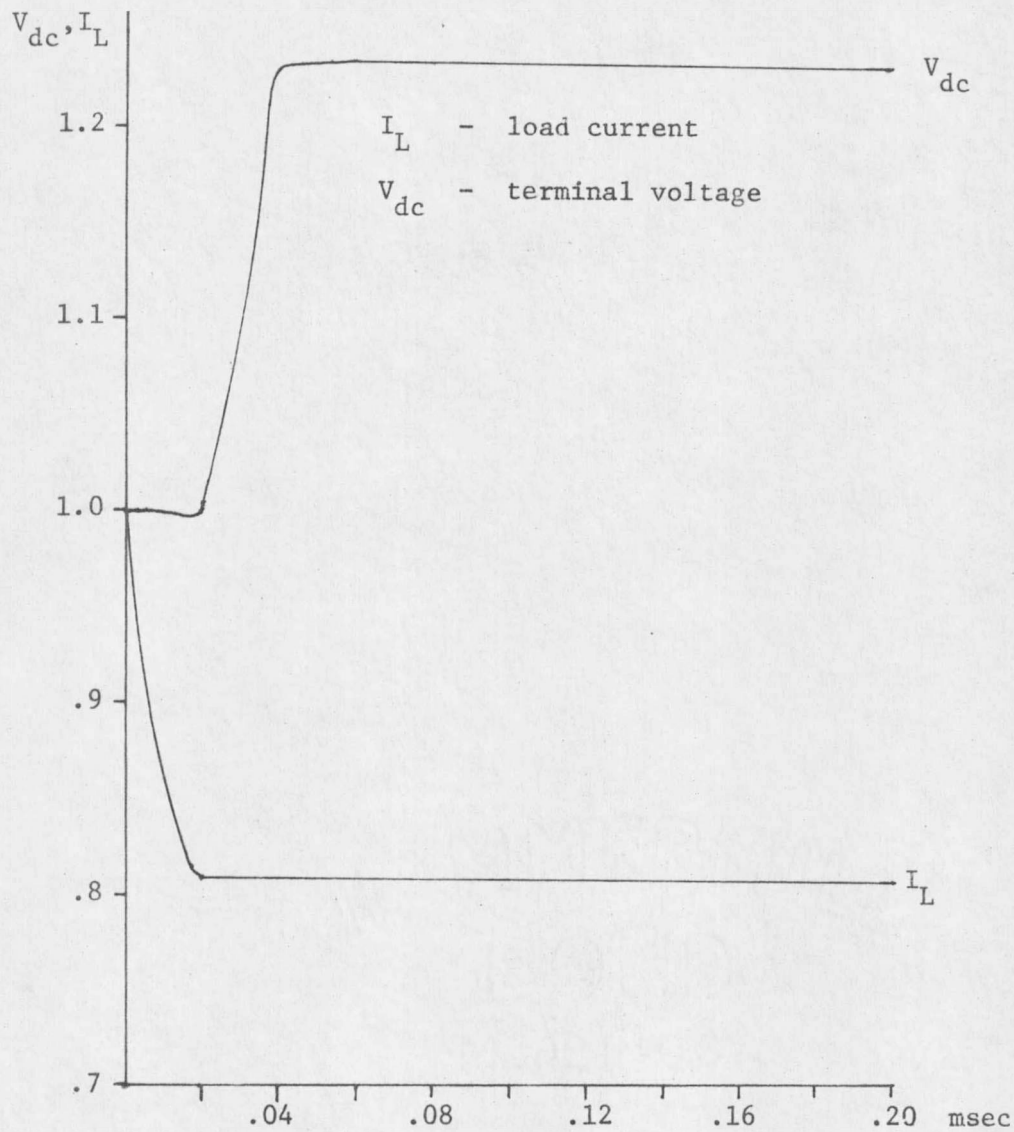


Figure 5.10b. Response of the MHD Generator to a + 52 % step change in the load resistance (V_{dc} and I_L are normalized with the values at $t = 0$). (Expanded time scale)

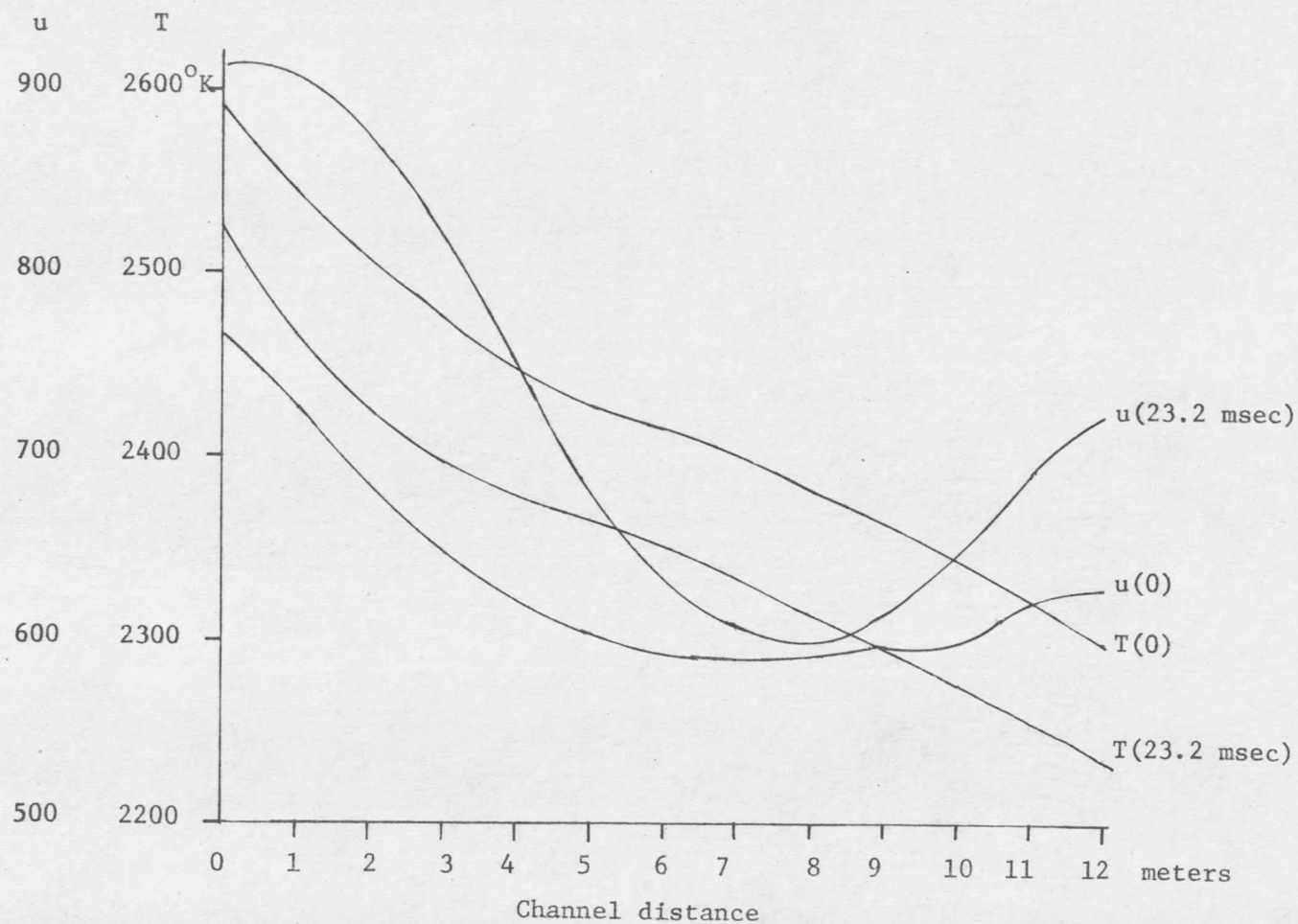


Figure 5.11. Gasdynamic parameters at $I_L = 16000$ ($t=0$) and at $I_L = 12480$ amperes ($t=23.2 \text{ msec}$)

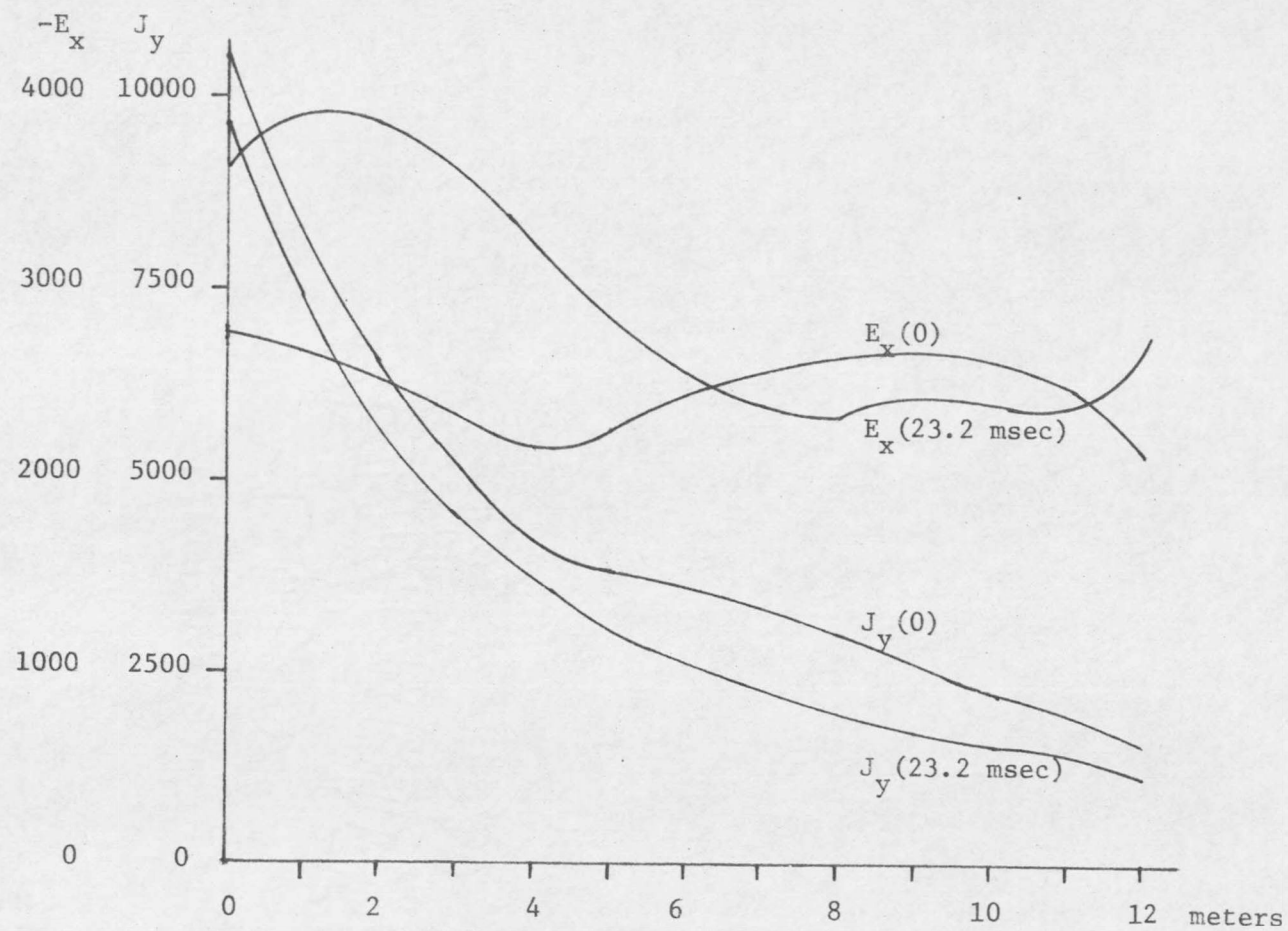


Figure 5.12. Variation of E_x and J_y with channel distance and time.

fluid was converted to kinetic energy as indicated by the decrease in the gas temperature and an increase in the gas velocity.

5.2.2 MHD Generator Coupled to a DC to AC Power Converter

The operating conditions of the MHD generator (completely described in Table 5.1) are:

Load current (I_L)	- 16000 amperes
Terminal voltage (V_{dc})	- 29568 volts
Internal resistance (R_i)	- 2.25 ohms
Power output (P_o)	- 472 megawatts

The DC to AC converter designed for this generator (not optimized) has the following parameters:

Transformer line voltage	- 27000 volts
Inverter ignition delay angle	- 138°
Overlap angle	- 13.5°
Commutating inductance	- .4 millihenry
Transformer resistance	- .03 ohm

The rate of change of the load current is largely determined by the inductance of the series inductor and the internal resistance of the MHD generator (L_d/R_i). The minimum commutating time constant is governed mainly by the (di/dt) capability of the thyristors which is approximately 10^8 amperes/second. Ripples in the current require smoothing by the use of harmonic filters so that the AC output is

approximately a sine wave.

No attempt was made to simulate the MHD generator coupled to a DC to AC converter. It is suggested that more information should be obtained from the simulation of the complete MHD channel (nozzle, generator and diffuser). This study considered only the generator and the diffuser portion of the channel.

5.2.3. Dynamic Response of the Diffuser

The diffuser was designed to reduce the velocity of the gas leaving the MHD generator and for efficient pressure recovery. The pressure of the gas at the diffuser exit should be high enough to provide a positive pressure between the steam plant inlet (gas side) and the atmosphere. Under steady state condition, the pressure at the generator exit is equal to .987 atmosphere. At the diffuser exit, the pressure increased to 1.28 atmospheres which is sufficient to take care of the pressure drop in the steam plant and the air heaters.

A +52 % step change in the generator load resistance resulted in a generator exit pressure of .95 atmosphere and a diffuser exit pressure of 1.24 atmospheres. The variation of the gas velocity and gas temperature in the diffuser are shown in Figure 5.13. A large reduction in the power output (decrease in the load current) of the generator may cause a large drop in the generator exit pressure (see Figure 5.6).

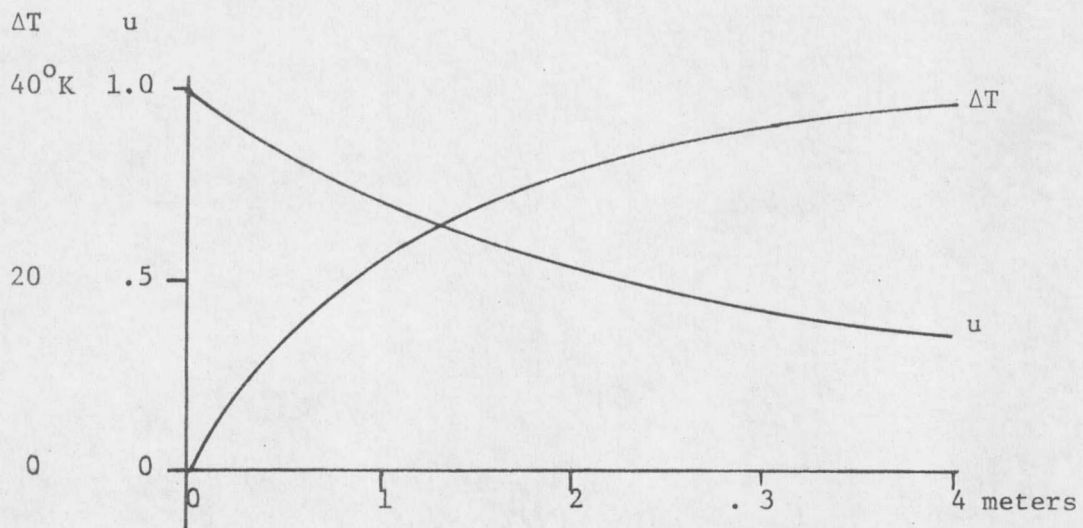
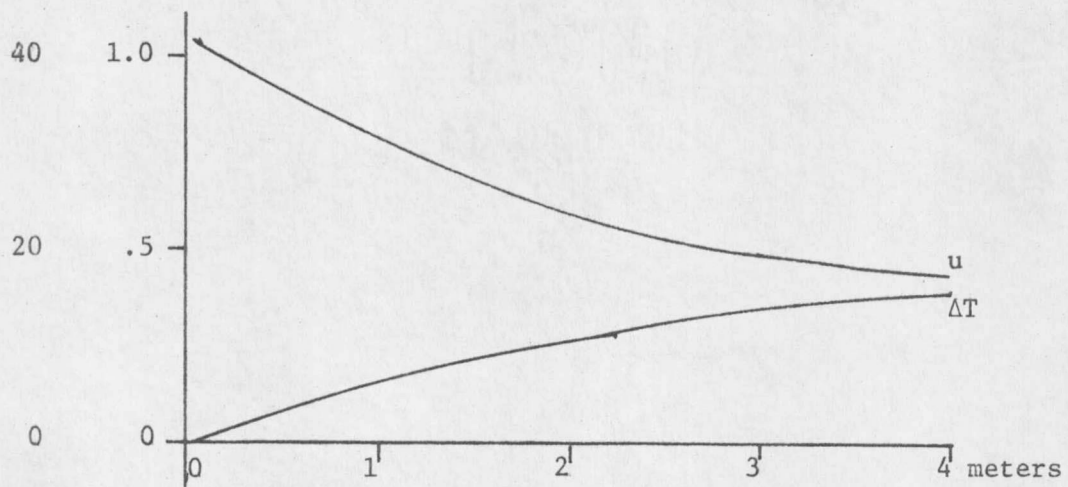
(a) $t = 0$ (b) $t = 23.2$ milliseconds

Figure 5.13. Variation of the gas velocity (normalized) and the gas temperature (ΔT) with the channel distance in the diffuser and time.

Under this condition a longer diffuser is necessary. The diffuser should be designed to take care of this abnormal condition otherwise a draft fan is necessary to suck the gas out to the atmosphere.

5.3 Dynamic Response of the Steam Plant

The dynamic model of the steam plant includes four sections, the high temperature economizer, the radiant section, the primary superheater and the reheater. No effort was done to subdivide each section into a number of sub-sections (to obtain a more accurate result). Since the steam plant is in its conceptual stage, no comparison can be made with an existing system. The steady state design parameters of the steam plant are shown in Table 5.2. A complete listing of the steam plant design parameters is given in Appendix B.

The steam plant was simulated to determine its response to a variation in the input parameters (such as the combustion gas mass flow rate and temperature, and the mass flow rate of the steam or feed-water). The design parameters were based on the steady state operating conditions of a steam plant for a 2000 megawatt thermal input to the MHD system.

The state of the gas (temperature and pressure) entering the steam plant is determined by the operating conditions of the MHD generator and diffuser. The variations in the power output of the MHD

Table 5.2. Steady State Design Parameters of the Steam Plant

Gas Temperature , Inlet	-	2358 ^o K
Exit	-	420 ^o K
Water/Steam Temperature, Inlet	-	306 ^o K
Exit	-	822 ^o K (primary cycle)
	-	823 ^o K (reheat cycle)
Steam Pressure, High Pressure Turbine	-	2.409 x 10 ⁷ N/sq m , In
		4.13 x 10 ⁶ N/sq m , Out
Low Pressure Turbine	-	3.786 x 10 ⁶ N/sq m , In
		5.08 x 10 ⁴ N/sq m , Out
Steam Flow Rate, Primary cycle	-	404 Kg/sec
Reheat cycle	-	387 Kg/sec
Power Output	-	587 megawatts
Air Compressor Power Requirement	-	152.5 megawatts

will cause a corresponding variations in the dynamic and thermodynamic parameters of the gas. A reduction in the power output of the MHD generator (with no reduction in the mass flow rate) may be large enough to produce a large increase in the velocity of the gas (see Figure 5.3-5.6). If supersonic speed is reached before the gas enters the diffuser, the subsonic diffuser will act as a nozzle, thereby accelerating further the gas flow. A shock may also occur, which is characterized by the sudden drop in the gas velocity and the sudden increase in the gas pressure and temperature (Figure 5.3-5.6).

The variation of the gas temperature, pressure and velocity greatly affects the performance of the steam plant. Over-exposure to adverse temperature, temperature gradient and gas velocity may cause failure or erosion of the metallic parts or even a reduction in the clearance between tubes. It is therefore necessary to obtain the transient response of the steam plant components to the variations in the gas flow parameters.

5.3.1 Results of the Simulation - Steam plant

An open-loop transient response was obtained for a step change in the combustion gas temperature entering the steam plant with the steam flow rate fixed. The variation of the metal temperature of the radiant section, superheater and reheater to a +5 % step change in the gas temperature and to a +5 % step change in the gas mass flow rate

are shown in Figures 5.14 to 5.16. The variation of the throttle pressure and temperature are also shown. In Figure 5.14, a + 5 % step change in the combustion gas temperature at the inlet of the radiant section produces a 24°K (3.3 %) increase in the metal temperature of the radiant section. The time constant for this section is approximately 60 seconds. The metal temperature of the primary superheater increased by 12°K (1.4 %) with a time constant of approximately 220 seconds. For the reheater metal, the temperature increased by 3°K (.35 %) with a time constant of approximately 260 seconds. The throttle temperature increased by 13°K with a time constant of approximately 150 seconds and the throttle pressure dropped by 2.5 % (time constant of approximately 90 seconds).

A + 5 % step change in the gas mass flow rate produced a very small increase in the radiant section metal temperature (less than 1°K). The effect on the superheater metal temperature is comparable with that of the + 5 % step change in the gas temperature. However, the effect on the reheater metal temperature is three times as much. The throttle temperature (high pressure turbine) increased by 9°K compared to 13°K for a + 5 % step change in the gas temperature, while a very small decrease in the throttle pressure was noted (.15 %).

To prevent excessive increase in the metal temperature, the steam flow rate was increased and at the same time the governor valve opening was increased to produce more power. Ignoring the transit

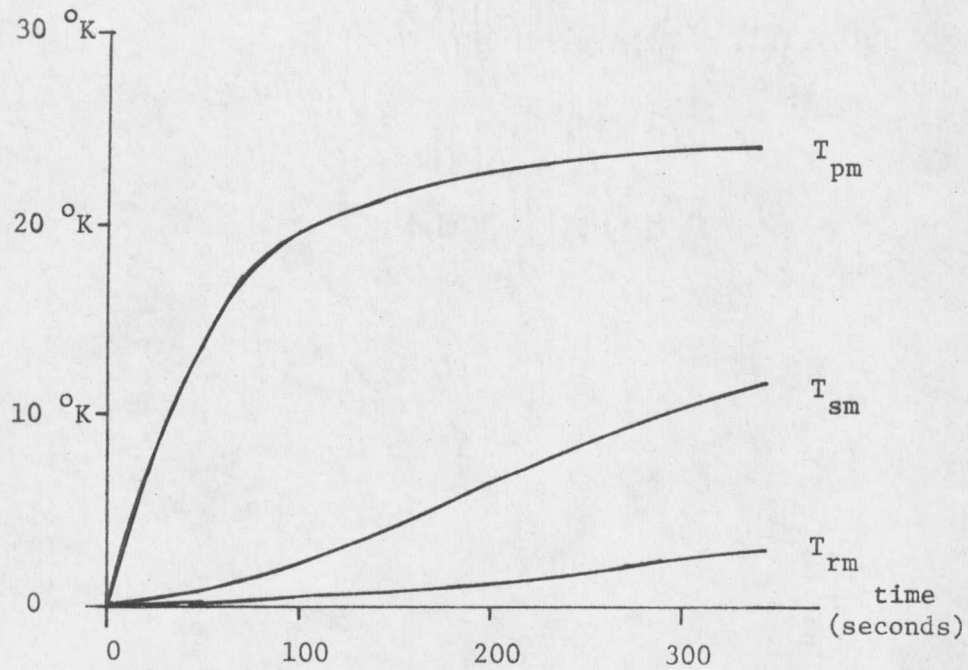


Figure 5.14. Open-loop response of the steam plant to a +5 % step change in the combustion gas temperature at the inlet of the Radiant section.

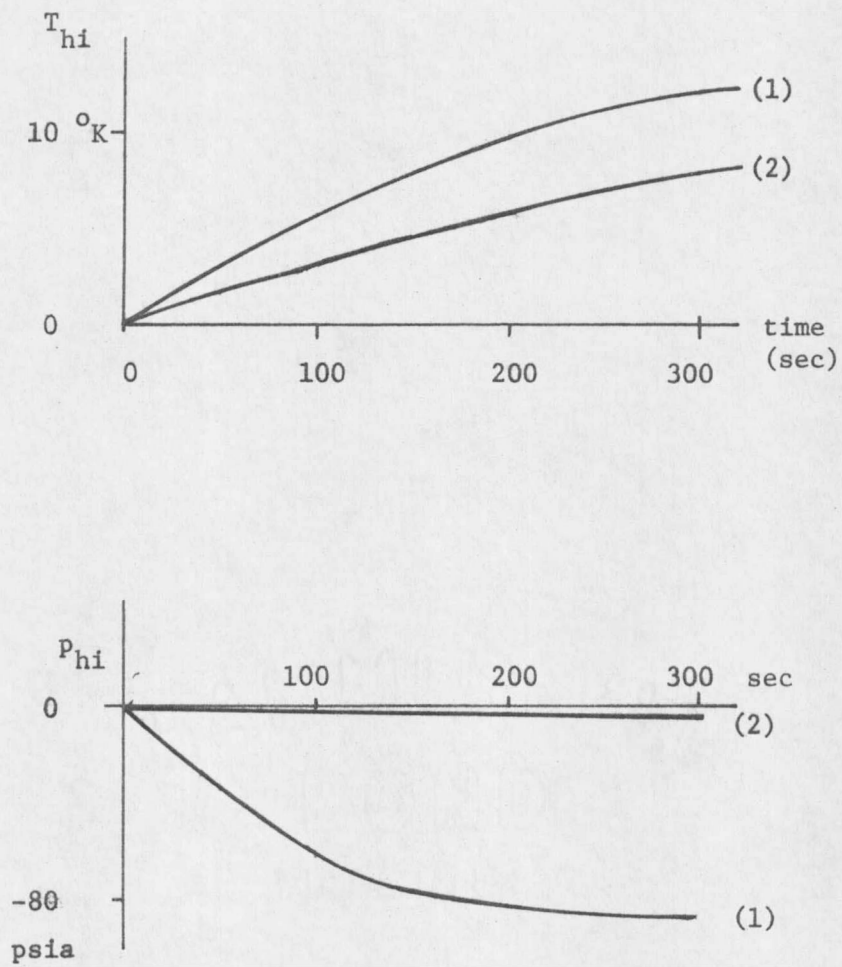


Figure 5.15. Response of the steam plant to a
 (1) +5 % step change in the
 combustion gas temperature and to
 a (2) +5 % step change in the gas
 mass flow rate.

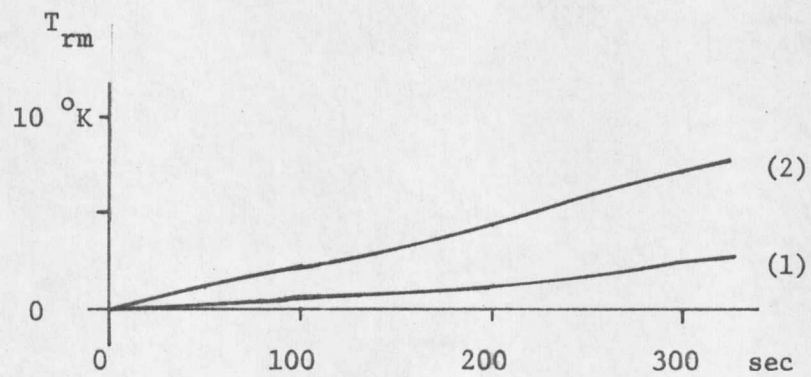
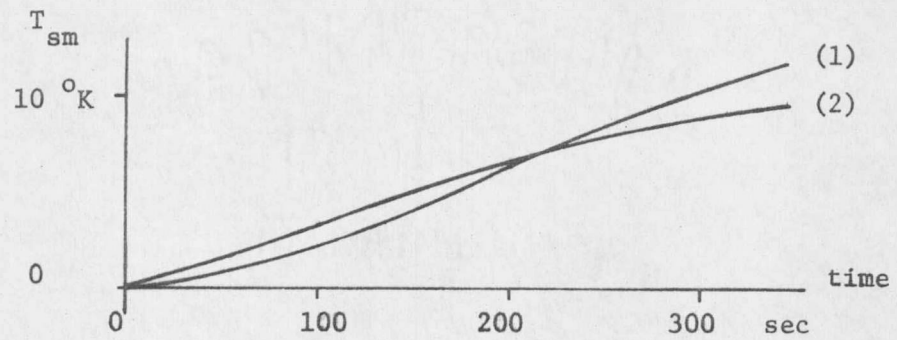


Figure 5.16. Open-loop response of the steam plant to a +5 % step change in (1) the combustion gas temperature and (2) the combustion gas mass flow rate.

time of the water/steam through the plant, the response of the steam plant to a + 5 % step change in the gas combustion temperature, a + 5 % step change in the steam mass flow rate and a 5 % step change in the governor valve opening was obtained. The results are shown in Figures 5.17 to 5.18. The radiant section metal temperature increased by 20°K (time constant of approximately 80 seconds). The increase in the superheater metal temperature is 3°K (approximately 1800 seconds time constant).

The steam plant can safely accomodate small variations of the gas temperature and mass flow rate for long periods of time by varying the feedwater flow rate. The large variations in the gas temperature for a short period of time (less than the time constant of the steam plant metal components) can also be safely handled. However, a very large increase for a longer period of time may be detrimental to the metal parts of the plant. Thus, it is important that the MHD generator should not be operated with low power output over a long period of time unless there is a corresponding decrease in the gas mass flow rate.

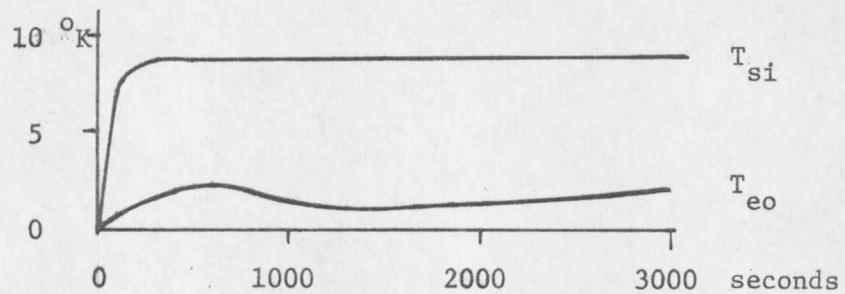
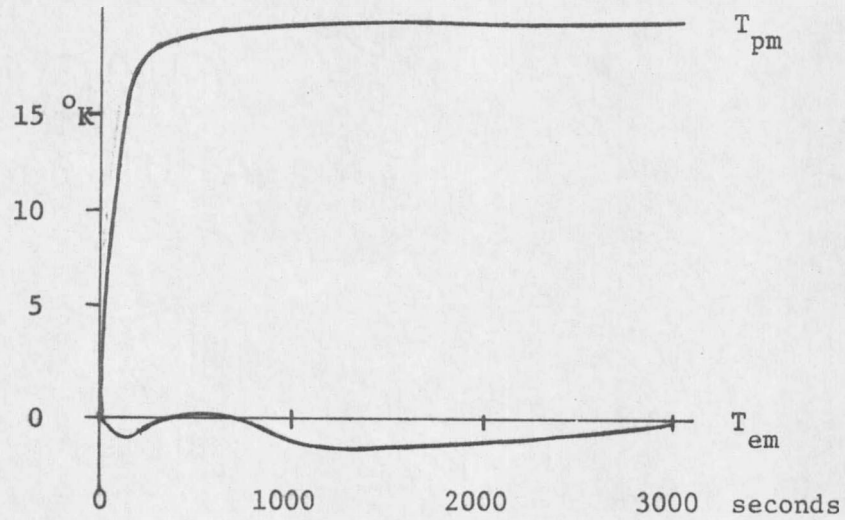


Figure 5.17. Response of the Steam Plant to a + 5 % Step Change in the Combustion Gas Temperature, the Steam Mass Flow Rate and the Governor Valve Opening

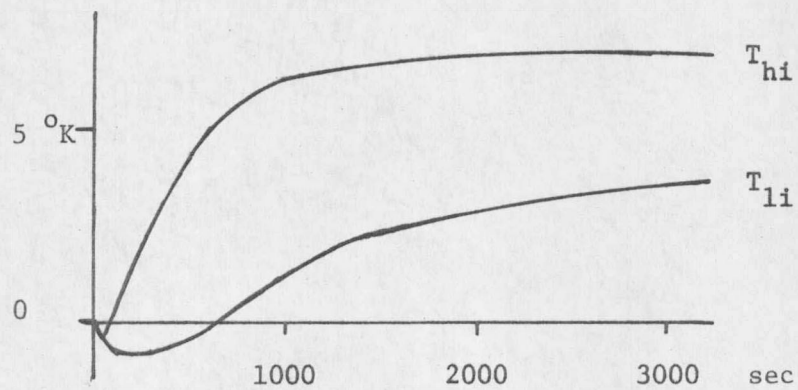
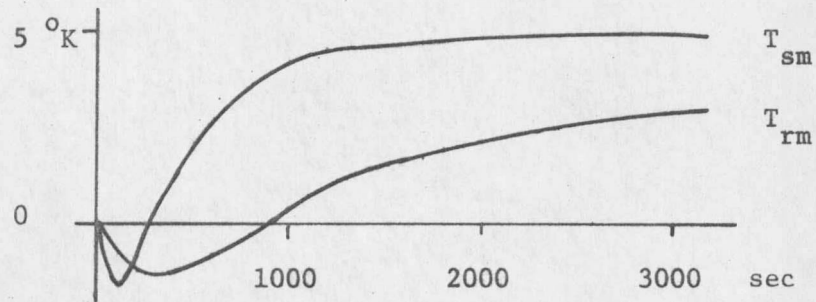


Figure 5.18. Response of the steam plant to a +5 % step change in the combustion gas temperature, the steam mass flow rate, and the governor valve opening.

Chapter VI

SUMMARY AND SUGGESTED FUTURE RESEARCH

6.0 Introduction

This chapter summarizes the results obtained and describes the conclusions drawn from the research study conducted. The significance of the findings is discussed, and the possible extensions of the research work are mentioned.

6.1 Summary and Conclusions

The problem of time-dependent MHD flow is of considerable interest because of the prospect of the development of MHD electrical power generation. The understanding of the transient and the steady state behavior of the system components is necessary in the design of the control mechanism for the system.

The mathematical model of the system was developed applicable to large power generation systems. The flow of a real gas (combustion products of coal, air, oxygen and seed) at subsonic conditions (typical for large systems) was considered. Only the open-loop operation was considered, thus the operation of the various control mechanisms were not included in the model.

The dynamic response of the MHD generator to load changes was obtained. The fast response of the MHD generator to load changes and

and the linear voltage-current characteristic (over a limited region) make it compatible with a DC to AC power converter. The rate of change of the load current can be limited by the series inductor. However, further investigation is required especially in the simulation of the MHD generator coupled to a DC to AC converter. Since the rate of change of the generator internal resistance is smaller than the rate of change of the load current and the load voltage (for pure resistance load), it is important to determine the effect of the generator internal resistance on the rate of change of the load current if the generator is coupled to a DC to AC converter. Likewise, it is important to determine the correlation between the rate of change of the generator internal resistance and the changes in the mass flow rate of the working fluid.

The power output of the MHD generator should be maintained such that the velocity of the gas at the generator exit does not exceed the velocity of sound. A large increase in the velocity at the generator exit also results in the decrease of the gas temperature, thus a possible decrease in the steam plant efficiency is obtained. It is also important to maintain a certain level of diffuser exit pressure to ensure a positive pressure between the steam plant inlet and exit (gas side).

Only the dynamic response of the steam plant to changes in the

input conditions (gas temperature, gas mass flow rate, steam flow rate and governor valve area) was considered in this study. It was shown that certain variations in the gas parameters at the inlet of the steam plant caused adverse effects on the physical component of the plant. Large variations of these parameters must be avoided to prevent plant failure. The variation of the power drawn from the MHD system should be a proportional change in both the steam plant and the MHD generator to avoid adverse effects including a large drop in system efficiency.

The simulation of the MHD generator and diffuser required large storage and computation time. The calculation of one time step required 4.8 milliseconds. Thus 80 minutes of computer time was required to simulate 20 milliseconds of real time, a ratio of 2.4×10^5 . The core storage required was 15000 words. Inclusion of the nozzle in the model would increase the core storage and the computation time by as much as 25 - 30 %.

It is uneconomical and impractical to simulate the MHD generator coupled with the steam plant because the time response of the steam plant components is of the order 10^5 larger than the time response of the MHD generator.

A more accurate results (in the gas dynamic and electrical parameters) can be obtained if the complete MHD channel (nozzle,

generator and diffuser) is simulated. Further investigation is needed in the design of the channel especially in matching the nozzle with the generator and the generator with the diffuser to avoid discontinuity in the gas parameters.

6.2 Suggestions for Future Research

The following areas appear to be fruitful for future work:

- (1) A computer simulation of the MHD channel which includes the nozzle, the generator and the diffuser. The dynamic response to variations in the input and output conditions should be investigated.
- (2) The possibility of using hybrid computation technique in the simulation of the time-dependent flow in the MHD channel.
- (3) A study of the different numerical techniques specifically for MHD electrical power generation (explicit or implicit methods).
- (4) A simulation of the MHD generator coupled to a DC to AC converter operating under normal and abnormal conditions.
- (5) A simulation of the complete MHD system in which the generator is coupled to a DC to AC converter with the converter coupled to the steam plant turbine-generator. The complete system may be coupled to a power transmission grid.
- (6) A more accurate modelling and simulation of the steam plant by subdividing each component into several sections.

APPENDIX

APPENDIX A

THERMODYNAMIC AND ELECTRICAL PROPERTIES OF THE WORKING FLUID

The calculation of the thermodynamic and electrical properties of the working fluid (coal, air and alkali seed) was based on the chemical equilibrium equation for carbon, oxygen, nitrogen and hydrogen mixtures [100]. Since the effect of the minor constituents of the working fluid have not been fully investigated, simplified equations were used in this study. The units used are in International Units (SI).

The specific enthalpy of the combustion gas is given by

$$h(T,p) = \left[\frac{4.187 \times 10^3}{(8\xi + 4)\phi + 32 + 28\psi} \right] \{ T[2 + 2(7 - 4\xi)\phi + 7\psi + 5Y + 3U] + \left[\frac{2 T_v}{e^{T_v/T} - 1} \right] [4 + (2 - 3\xi)\phi + \psi - 3Y - U] + (117 + 30\xi)(Y + \phi - 1) \times 10^3 \} + 135 \cdot \xi \cdot U \times 10^3 \quad ; \quad \text{for } \phi \geq 1$$

$$\begin{aligned}
 h(T,p) = & \left[\frac{4.187 \times 10^3}{(8\xi + 4)\phi + 32 + 28\psi} \right] \cdot T \cdot [7 + (9 - 8\xi)\phi + \\
 & 7\psi + 5Y + 3U] + \left[\frac{2 T_v}{e^{T_v/T} - 1} \right] [1 + (5 - 3\xi)\phi + \\
 & \psi - 3Y - U] + (117 + 30\xi) \cdot (Y + \phi - 1) \times 10^3 + \\
 & 135 \cdot \xi \cdot U \times 10^3 \quad ; \quad \text{for } \phi < 1
 \end{aligned}$$

where $\xi = 4\delta / (1 + 4\delta)$

$$T_v = (3000 - 2000\xi + 300\psi) / (1 - .5\xi + .09\psi)$$

δ - carbon to hydrogen ratio of the fuel

ψ - nitrogen to oxygen mole ratio of the combustion gas

ϕ - fuel mixture ratio (actual fuel to oxygen ratio/
stoichiometric fuel to oxygen ratio)

The extra number of molecules due to the dissociation of the triatomic molecules into diatomic molecules is

$$Y = \frac{X [1 + (1 - \phi)/X + (.36([1 - \phi]/X)^2)]}{[1 + .36(1 - \phi)/X]} \quad ; \quad \text{for } \phi < 1$$

$$= \frac{X}{[1 + .64(\phi - 1)/X + .3([\phi - 1]/X)^2]} \quad ; \quad \text{for } \phi \geq 1$$

The extra number of molecules due to the dissociation of the diatomic molecules into monatomic molecules is

$$U = \frac{(1 - 2.\xi.X) (2 - \xi + \psi)}{[4.X.K_1.K_2.p (.985 \times 10^{-5})]}$$

where $X = Y$ for $\phi = 1$

$$X = \frac{A [3(2 - \xi + \psi) + A.\xi(5 - 2\xi + 2\psi)]}{3(1 + 2.A.\xi) (2 - \xi + \psi) + 2.A^2.\xi^2(5 - 2\xi + 2\psi)} ;$$

for $\phi \leq 1$

$$\text{and } A = \left[\frac{2 - \xi + \psi}{4.K_1^2.p(.987 \times 10^{-5})} \right]^{1/3}$$

$$K_1 = .39 \times 10^{-4} e^{(-.3\xi + 34000/T)}$$

$$K_2 = .14 \times 10^{-3} e^{(1.3\xi + 29000/T)}$$

The specific enthalpy of the fuel, the oxidant and the seed before combustion is

$$h_{bc}(T) = W_a.h_a(T_{pa})/W + W_o.h_{ox}(T_o)/W +$$

$$[(8\xi + 4)\phi.Fuel - 8.79 \times 10^7 (1 - \xi) -$$

$$Ed(8\xi + 4)\phi.Seed]/[(8\xi + 4) + 32 + 28\psi]$$

- where W_a - mass flow rate of the air, Kg/sec
 W - mass flow rate of the combustion gas, Kg/sec
 W_o - mass flow rate of the oxygen, Kg/sec
 h_a - specific enthalpy of air, Joules/Kg
 h_{ox} - specific enthalpy of oxygen, Joules/Kg
 Ed - dissociation energy of the seed, Joules/Kg
 $Fuel$ - gross calorific value of the fuel, Joules/Kg
 T_{pa} - preheat temperature of air, °Kelvin
 T_o - temperature of oxygen, °Kelvin
 $Seed$ - seed concentration in weight potassium/weight fuel
 Q - seed concentration in mole % potassium
and $Ed = [Type + 1.1 T/200] \times 10^6$
 $Type = -2.3$ for K_2CO_3
 $= 3.4$ for K_2SO_4
 $Seed = 39 Q [(Z_1 - \xi)\phi + Z_2 + \psi] / [100 \phi (8\xi + 4)]$
 $Z_1 = 1$
 $Z_2 = 1$ } for $\phi < 1$
 $Z_1 = 2$
 $Z_2 = 0$ } for $\phi \geq 1$

The molecular weight of the combustion gas is

$$M = \frac{(8\xi + 4) + 32 + 28\psi}{1 + (1 - \xi)\phi + \psi + Y + U} \quad ; \text{ for } \phi < 1$$

$$= \frac{(8\xi + 4)\phi + 32 + 28\psi}{(2 - \xi)\phi + \psi + Y + U} \quad ; \text{ for } \phi \geq 1$$

The equation of state is

$$p = \rho T / (M \times .1218 \times 10^{-3})$$

where p - pressure of the combustion gas, Newtons/sq m

T - temperature of the combustion gas, °Kelvin

ρ - mass density of the combustion gas, Kg/cu m

The electrical properties of the combustion gas are

The electron density is

$$\eta_e = \frac{4.22 \times 10^{23} [Q(p \times .987 \times 10^{-5}) T^{.5} e^{-50400/T}]^{.5}}{1 + 2 \times 10^{-3} (e^{16800/T}) (Y/X)^{1/3} B^{2/3}}$$

where $B = (1 - \xi) (.987 \times 10^{-5} p) / (2 - \xi + \psi)$

The electron mobility is

$$\mu = T / \{ [p \times .987 \times 10^{-5}] [(4030 - 3110\xi) (2.72 - .457\psi) + (.15 + .3\xi) (.66\psi - 1.48) T + (1050 - 120\psi) Q + (.05\psi - .13) T.Q + (1.1 + 3.7\psi) (\eta_e \times 10^{-19})] \}$$

and the electrical conductivity is

$$\sigma = 1.6 \times 10^{-19} \eta_e \mu$$

APPENDIX B

DESIGN PARAMETERS OF THE STEAM PLANT AND AIR HEATERS

The steam plant and air heaters were designed for a 2000 MW thermal input to the MHD combustor. The power output of the steam plant is approximately 55 % of the total power output of the system.

The thermal duty of each steam plant section was chosen such that each section is located at the temperature zone where the gas temperature is high enough to provide good heat transfer from the gas to the steam yet not so high as to result in excessive tube temperature.

The heat transfer equation for heat flow is [127]

$$q = US\Delta T \quad (B.1)$$

where q - rate of heat flow, Joules/sec

U - overall combined conductance, Joules/sq m, sec, $^{\circ}\text{K}$

S - heat transfer surface area, sq m

ΔT - temperature difference causing heat flow, $^{\circ}\text{K}$

The overall combined conductance (U) includes fluid convection c conductance and intertube radiation conductance. The equation (B.1) may be expanded as follows, to show the quantitative relationship in the transfer of heat from a heating to a cooling fluid:

$$\begin{aligned} q &= US\Delta T_m = wc(T_1 - T_2) = w'c'(T_2' - T_1') \\ &= w'(h_2' - h_1') \end{aligned} \quad (B.2)$$

where T_m - mean temperature difference between the heating and the cooling fluid (see chapter IV of reference [127].

w - mass flow rate of hotter fluid, Kg/sec

c - mean specific heat of hotter fluid, Joules/Kg, $^{\circ}\text{K}$
(normally c_p)

c_p - specific heat of the fluid at constant pressure

T_1, T_2 - temperature of hotter fluid entering and leaving the heat transfer surface, $^{\circ}\text{K}$

w' - mass flow rate of colder fluid, Kg/sec

c' - specific heat of colder fluid

T'_1, T'_2 - temperature of colder fluid entering and leaving the heat transfer surface

h'_1, h'_2 - specific enthalpy of the colder fluid entering and leaving the heat transfer surface, Joules/Kg

The inlet and exit temperature (of gas and steam) in each section were calculated using equation B.2. The gas temperature leaving the last section of the steam plant (economizer) was assumed to be 420°K which is high enough to prevent condensation of sulfuric acid on the economizer tube wall [127].

B.1 Low Temperature Economizer

Thermal duty	- 93000000 Joules/sec
Tube size	- 5.1 cm OD, .635 cm thick
Surface area	- 12860 sq m
Volume of water	- 92.5 cu m
Volume of metal	- 71.5 cu m
Density of metal	- 6600 Kg/cu m
Mass of metal	- 470470 Kg
Metal specific heat	- 3186 Joules/Kg
Gas Inlet temperature	- 548 °K
Exit temperature	- 420 °K
Mass flow rate	- 687.4 Kg/sec
Water Inlet temperature	- 355 °K
Exit temperature	- 412 °K
Mass flow rate	- 387 Kg/sec

B.2 High Temperature Economizer

Thermal duty	- 96000000 Joules/sec
Tube size	- 5.1 cm OD, .635 cm thick
Surface area	- 11840 sq m
Volume of water	- 85.3 cu m
Volume of metal	- 65.6 cu m
Mass of metal	- 432000 Kg

Gas Inlet temperature	- 671 °K
Exit temperature	- 548 °K
Mass flow rate	- 687.4 Kg/sec
Water Inlet temperature	- 473 °K
Exit temperature	- 525 °K
Mass flow rate	- 404 Kg/sec

B.3 Diffuser Cooling Tubes

Thermal duty	- 21500000 Joules/sec
Tube size	- 2.635 cm OD, .635 cm thick
Volume of steam	- 1.144 cu m
Gas Inlet temperature	- 2299.5 °K
Exit temperature	- 2358 °K
Mass flow rate	- 687.4 Kg/sec
Steam Inlet temperature	- 525 °K
Exit temperature	- 536 °K
Mass flow rate	- 404 Kg/sec

B.4 Radiant Section

Thermal duty	- 300000000 Joules/sec
Tube size	- 3.8 cm OD, .635 cm thick
Surface area	- 1350 sq m
Volume of steam	- 5.7 cu m

Volume of metal	- 7.14 cu m
Mass of metal	- 47100 Kg
Gas Inlet temperature	- 2358 °K
Exit temperature	- 2173 °K
Mass flow rate	- 687.4 Kg/sec
Steam Inlet temperature	- 536 °K
Exit temperature	- 651 °K
Mass flow rate	- 404 Kg/sec

B.5 Primary Superheater

Thermal duty	- 584800000 Joules/sec
Tube size	- 3.8 cm OD, .635 cm thick
Surface area	- 8310 sq m
Volume of steam	- 35.1 cu m
Volume of metal	- 43.7 cu m
Gas Inlet temperature	- 1826 °K
Exit temperature	- 1213 °K
Mass flow rate	- 687.4 Kg/sec
Steam Inlet temperature	- 651 °K
Exit temperature	- 822 °K
Mass flow rate	- 404 Kg/sec

B.6 Reheater

Thermal duty	- 261000000 Joules/sec
Tube size	- 5 cm OD, .635 cm thick
Surface area	- 14090 sq m
Volume of steam	- 94 cu m
Volume of metal	- 74.6 cu m
Mass of metal	- 492300 Kg
Gas Inlet temperature	- 1213 °K
Exit temperature	- 919 °K
Mass flow rate	- 687.4 Kg/sec
Steam Inlet temperature	- 550 °K
Exit temperature	- 822 °K
Mass flow rate	- 387 Kg/sec

B.7 Low Temperature Air Heater

Thermal duty	- 200000000 Joules/sec
Tube size	- 5.1 cm, .325 cm thick
Surface area	- 26300 sq m
Volume of air	- 254 cu m
Volume of metal	- 80 cu m
Mass of metal	- 528000 Kg

Gas Inlet temperature	- 919 °K
Exit temperature	- 671 °K
Mass flow rate	- 687.4 Kg/sec
Air Inlet temperature	- 516 °K
Exit temperature	- 838 °K
Mass flow rate	- 580 Kg/sec

B.8 High Temperature Air Heater

Thermal duty	- 436000000. Joules/sec
Number of units	- 12

6 on gas, 3 on air, 2 on pressurization and depressurization during switchover and 1 on standby

Matrix height	- 28.5 cm
Base area	- 37 sq m
Passage size	- 15 cm x 15 cm
Pressure Shell height	- 48 m
Pressure shell diameter	- 9.15 m
Gas Inlet temperature	- 2173 °K
Exit temperature	- 1826 °K
Mass flow rate	- 687.4 Kg/sec
Air Inlet temperature	- 838 °K
Exit temperature	- 1480 °K
Mass flow rate	- 580 Kg/sec

APPENDIX C

THERMODYNAMIC PROPERTIES OF STEAM/WATER

The thermodynamic properties of steam/water were obtained from reference [152]. The mass density and the temperature are expressed in terms of the specific enthalpy and the pressure. The method of Least Squares was used to obtain the desired polynomial expression for each region of interest with errors limited to less than .2 %.

C.1. The Economizer and the Diffuser wall Cooling Tubes

$$T = a_0 + a_1 h + a_2 h^2 + a_3 h^3$$

$$\rho = c_0 + c_1 h + c_2 h^2 + c_3 h^3 + d_1 p + d_2 p^2 + d_3 p^3$$

where $a_0 = 268.51$	$c_2 = 2.455216 \times 10^{-4}$
$a_1 = 2.3209 \times 10^{-4}$	$c_3 = -8.0570184 \times 10^{-17}$
$a_2 = 2.13519 \times 10^{-11}$	$d_1 = 3.388339 \times 10^{-6}$
$a_3 = -1.7977 \times 10^{-17}$	$d_2 = -4.16304 \times 10^{-15}$
$c_0 = 1181.23$	$d_3 = -1.1045783 \times 10^{-21}$
$c_1 = -5.6162 \times 10^{-4}$	

$$\text{for } 400 < T < 550 \text{ } ^\circ\text{K} \quad ; \quad 3400 < p < 4200 \text{ psia}$$

C.2. The Radiant Boiler Tubes

$$T = a_0 + a_1 h + a_2 h^2 + a_3 h^3 + b_1 p + b_2 p^2 + b_3 p^3$$

$$\rho = c_0 + c_1 h + c_2 h^2 + c_3 h^3 + d_1 p + d_2 p^2 + d_3 p^3$$

where

$$a_0 = 252.77$$

$$c_0 = 1848.$$

$$a_1 = 1.84876 \times 10^{-4}$$

$$c_1 = -1.65883 \times 10^{-3}$$

$$a_2 = 6.327195 \times 10^{-11}$$

$$c_2 = 9.927461 \times 10^{-10}$$

$$a_3 = -3.0705698 \times 10^{-17}$$

$$c_3 = 2.553673 \times 10^{-16}$$

$$b_1 = 2.130739 \times 10^{-6}$$

$$d_1 = -1.54802 \times 10^{-6}$$

$$b_2 = -2.441379 \times 10^{-14}$$

$$d_2 = -2.305187 \times 10^{-13}$$

$$b_3 = -3.1984747 \times 10^{-22}$$

$$d_3 = 8.2748951 \times 10^{-21}$$

for $500 < T < 670$ °K ; $3200 < p < 4000$ psiaC.3. The Primary Superheater Tubes

The coefficients are:

$$a_0 = 901.28$$

$$c_0 = 964.7$$

$$a_1 = -2.43656 \times 10^{-4}$$

$$c_1 = -3.52043 \times 10^{-4}$$

$$a_2 = -9.155152 \times 10^{-12}$$

$$c_2 = 2.353127 \times 10^{-12}$$

$$a_3 = 1.88961 \times 10^{-17}$$

$$c_3 = 8.6834533 \times 10^{-18}$$

$$b_1 = 6.804904 \times 10^{-6}$$

$$d_1 = 7.9417977 \times 10^{-6}$$

$$\begin{aligned}
 b_2 &= -6.8024416 \times 10^{-14} & d_2 &= -1.0829445 \times 10^{-12} \\
 b_3 &= 4.90753777 \times 10^{-22} & d_3 &= 2.67533636 \times 10^{-20}
 \end{aligned}$$

for $620 < T < 830$ °K ; $3200 < p < 3800$ psia

C.4. The Reheater Tubes

The coefficients are:

$$\begin{aligned}
 a_0 &= 449.22 & c_0 &= 5.029 \\
 a_1 &= -1.06938 \times 10^{-5} & c_1 &= 3.24766 \times 10^{-6} \\
 a_2 &= -3.848692 \times 10^{-11} & c_2 &= -6.087039 \times 10^{-13} \\
 a_3 &= 1.96655242 \times 10^{-17} & c_3 &= 1.5578034 \times 10^{-19} \\
 b_1 &= 2.195649 \times 10^{-6} & d_1 &= 1.7334559 \times 10^{-6} \\
 b_2 &= 3.6871222 \times 10^{-13} & d_2 &= 1.43081998 \times 10^{-13} \\
 b_3 &= -1.61763505 \times 10^{-20} & d_3 &= -4.94395512 \times 10^{-21}
 \end{aligned}$$

for $530 < T < 830$ °K ; $520 < p < 650$ psia

C.5. The High Pressure Steam Turbine Exhaust

The coefficients are:

$$\begin{aligned}
 a_0 &= 268.55 & c_0 &= 4.67 \\
 a_1 &= 2.10955 \times 10^{-4} & c_1 &= 8.1106 \times 10^{-7} \\
 a_2 &= 2.685907 \times 10^{-11} & c_2 &= -2.244634 \times 10^{-11} \\
 a_3 &= 3.903805 \times 10^{-18} & c_3 &= -2.297003 \times 10^{-19}
 \end{aligned}$$

$$\begin{aligned} b_1 &= 4.895478 \times 10^{-5} & d_1 &= 1.9053 \times 10^{-5} \\ b_2 &= -9.939367 \times 10^{-12} & d_2 &= -3.874922 \times 10^{-12} \\ b_3 &= 8.65004945 \times 10^{-19} & d_3 &= 3.345578 \times 10^{-19} \end{aligned}$$

for $450 < T < 600$ °K ; $400 < p < 700$ psia

where h - specific enthalpy, Joules/Kg

p - pressure, Newtons/sq m

ρ - mass density, Kg/cu m

T - temperature , °K

APPENDIX D

THERMODYNAMIC AND TRANSPORT PROPERTIES OF AIR

The properties of air were obtained from reference [153].

D.1. Low Temperature Air Heater

$$T = a_0 + a_1h + a_2h^2 + a_3h^3 + b_1p + b_2p^2 + b_3p^3$$

$$Z = c_0 + c_1h + c_2h^2 + c_3h^3 + d_1p + d_2p^2 + d_3p^3$$

$$\rho = p / (286.1186 \times Z \times T)$$

where $a_0 = 772.09$

$$c_0 = .9403$$

$$a_1 = -1.73118 \times 10^{-3}$$

$$c_1 = 3.004735 \times 10^{-4}$$

$$a_2 = 3.199369 \times 10^{-9}$$

$$c_2 = -4.4398956 \times 10^{-7}$$

$$a_3 = -1.281605 \times 10^{-15}$$

$$c_3 = 2.1527 \times 10^{-10}$$

$$b_1 = 3.444268 \times 10^{-6}$$

$$d_1 = -2.990621 \times 10^{-8}$$

$$b_2 = -5.551004 \times 10^{-12}$$

$$d_2 = 4.98913045 \times 10^{-14}$$

$$b_3 = 2.61970912 \times 10^{-18}$$

$$d_3 = -2.315999 \times 10^{-20}$$

for $550 \leq T < 900$ °K ; $1 < p < 10$ atmospheres

D.2. High Temperature Air Heater

The coefficients are:

$$a_0 = 182.5$$

$$c_0 = 1.2184$$

$$a_1 = 6.95564 \times 10^{-4}$$

$$c_1 = -5.99173 \times 10^{-4}$$

$$a_2 = 1.220374 \times 10^{-10}$$

$$c_2 = 5.576015 \times 10^{-7}$$

$$a_3 = -3.45359 \times 10^{-17}$$

$$c_3 = -1.7155671 \times 10^{-10}$$

$$b_1 = 5.71180135 \times 10^{-6}$$

$$d_1 = -2.41645388 \times 10^{-8}$$

$$b_2 = -1.063745 \times 10^{-11}$$

$$d_2 = 4.15909067 \times 10^{-14}$$

$$b_3 = 5.3234325 \times 10^{-18}$$

$$d_3 = -1.98454265 \times 10^{-20}$$

for $1000 < T < 1800$ °K ; $1 < p < 10$ atmospheres

where T - air temperature, °Kelvin

Z - compressibility of air

p - air pressure, Newtons/sq m

ρ - air mass density, Kg/cu m

h - air specific enthalpy, Joules/Kg

APPENDIX E

SPECIFIC HEAT OF THE COMBUSTION GAS

The average specific heat of the combustion gas may be expressed as a linear function of the temperature at the inlet of the specified section. These equations were derived from the gas properties calculation as shown in the Appendix A. Since the pressure difference between the gas inlet and the gas outlet of the steam plant is small, the effect of the pressure variation on the specific enthalpy is negligible. The specific heat (constant pressure) equations are

E.1. Radiant Boiler

$$C_{gp} = 2045. + .4262(T_g - 2260.) \quad ; \quad 2000 < T < 2500 \text{ } ^\circ K$$

E.2. High Temperature Air Heater

$$C_{gah} = 1507.7 + .7199(T_{gp} - 2020.) \quad ; \quad 1400 < T < 2300 \text{ } ^\circ K$$

E.3. Primary Superheater

$$C_{gs} = 1315.7 + .2136(T_{gah} - 1540.) \quad ; \quad 800 < T < 1800 \text{ } ^\circ K$$

E.4. Reheater

$$C_{gr} = 1210.4 + .3537(T_{gs} - 980.) \quad ; \quad 600 < T < 1300 \text{ } ^\circ K$$

E.5. Low Temperature Air Heater

$$C_{gal} = 1128.9 + .4574(T_{gr} - 700) \quad ; \quad 450 \leq T \leq 1000 \text{ } ^\circ K$$

E.6. High Temperature Economizer

$$C_{ge} = 1073.6 + .4338(T_{gal} - 540.) \quad ; \quad 400 \leq T \leq 700 \text{ } ^\circ K$$

APPENDIX F

CALCULATION OF THE COMBUSTION TEMPERATURE

The combustion temperature is calculated from the energy balance equation [154]

$$h(T_c, p_c) = h_{bc}(T_c) - Q_c/W$$

where $h(T_c, p_c)$ - the enthalpy of the combustion products
 $h_{bc}(T_c)$ - the enthalpy of the fuel, the oxidant, and the seed before combustion

Q_c - heat loss to the combustion chamber walls

W - mass flow rate of the combustion products

The heat loss to the combustion chamber walls is given by

$$Q_c = \pi \cdot D_c \cdot L_c \{ C_{hs} [h(T_c, p_c) - h(T_{wc}, p_c) + S(\epsilon_g \cdot T_c^4 - \epsilon_{ws} \cdot T_{wc}^4)] + \pi \cdot D_c^2 \{ C_{he} [h(T_c, p_c) - h(T_{wc}, p_c)] + S(\epsilon_g \cdot T_c^4 - \epsilon_{we} \cdot T_{wc}^4) \} \}$$

where D_c - characteristic diameter of the combustion chamber, m
 L_c - length of the combustion chamber, m
 p_c - combustion pressure, Newtons/sq m

- T_c - combustion temperature, °Kelvin
 T_{wc} - design wall temperature, °Kelvin
 ϵ_g - emissivity of the combustion gas
 ϵ_{ws} - emissivity of the side walls
 ϵ_{we} - emissivity of the end walls
 C_{hs} - convective heat transfer coefficient on the side walls
 $\quad = G_{ms} \cdot Re^{.8} (k/D_c C_p)$
 C_{he} - convective heat transfer coefficient on the end walls
 $\quad = G_{me} \cdot Re^{.8} (k/D_c C_p)$
 $\frac{G_{ms}}{G_{me}}$ - empirical coefficient in the turbulent heat transfer correlation for the side walls and the end walls respectively
 Re - Reynolds number
 C_p - Specific heat (constant pressure) of the gas, Joules/
 k - Thermal conductivity of the gas

The cross-sectional area of the combustion chamber is calculated from the continuity equation

$$A_c = W / (\rho_c \cdot u_c)$$

The mass density (ρ_c) is calculated from the equation of state at the combustion temperature and pressure. It is assumed that

there is a negligible pressure drop in the combustion chamber.

The length of the combustion chamber is

$$L_c = u_c \cdot T_r$$

where u_c is the optimum velocity of the gas in the combustion chamber for a minimum surface area given by

$$u_c = [4W/(\pi \cdot \rho_c \cdot T_r)]^{1/3}$$

where T_r is the residence time for the complete combustion of coal and W is the mass flow rate of the combustion gas.

APPENDIX G

THE FORTRAN IV COMPUTER PROGRAMS

The computer simulations were done at the Montana State University Computing Center using the XDS Sigma 7 computer. The dynamic models were implemented by the use of the Fortran IV computer programming language. All arithmetic operations were performed in double precision to minimize truncation errors.

G.1 Program MHDTD

This program is used to solve the quasi one-dimensional time-dependent magnetohydrodynamics equations in conservation form developed in chapter II.

The two-step Lax-Wendroff finite difference method is used, making it possible to integrate through a discontinuity. The dependent variables are the mass density, the momentum density and the stagnation internal energy density. The independent variables are time and distance along the channel.

Nomenclature

A	-	cross-sectional area of the channel, meters
AA	-	width to height ratio of the channel
AMP	-	dimensionless current
AMP1	-	load current, amperes
BETA	-	Hall parameter
BN	-	applied magnetic field density, webers/sq m
C4	-	electrode pitch to channel width ratio
C5	-	electrode pitch, meters

D	-	hydraulic duct diameter, meters
DADX	-	slope of the area profile of the channel
DELTA	-	carbon to hydrogen ratio of the fuel
DIFF	-	diffuser length, meters
DTAU	-	time increment, seconds
DUCT	-	MHD generator length, meters
DX	-	channel length increment, meters
E	-	stagnation internal energy density of the gas, Joules/cu m
EDC	-	axial electric field, volts/meter
EFF	-	isentropic efficiency
ENT	-	internal energy of the gas, Joules/Kg
EOC	-	open-circuit axial electric field, volts/meter
EY	-	transverse electric field, volts/meter
F	-	viscous force, Newtons/cu meter
G	-	heat loss to the channel wall, Joules/cu meter
P	-	gas pressure, Newtons/sq meter
PHI	-	fuel mixture ratio
PMHD	-	MHD electrical power output, Joules/sec
PSI	-	nitrogen to oxygen mole ratio
Q	-	seed concentration in mole %
QMHD	-	heat loss to the wall of the MHD generator, Joule Joules/sec
QDIFF	-	heat loss to the diffuser wall, Joules/sec
R	-	load resistance, ohms
RI	-	internal resistance of the generator, ohms
RHO	-	mass density of the gas, Kg/cu meter
SIGMA	-	electrical conductivity, mho/meter
T	-	gas temperature, °Kelvin
TAU	-	time, seconds
TW	-	wall temperature, °Kelvin
V	-	velocity of the gas, meters/sec
VD	-	voltage drop parameter
VDC	-	generator terminal voltage, volts
VOC	-	generator open-circuit voltage, volts
W	-	momentum density, Kg/sq meter . sec
XJB	-	Lorentz force, Newtons/cu meter
XJE	-	electrical power density, Joules/sec.cu meter
XJX	-	axial current density, amperes/sq meter
XJY	-	transverse current density, amperes/sq meter
XKY	-	cross-connection parameter
XM	-	molecular weight of the gas
XMACH	-	gas flow mach number
XMAX	-	total channel length, meters

Input Data

The first four read statements read the following list according to the format (6F.0).

Cross-connection parameter (XKY)
 Width to height ratio of the channel (AA)
 Load Resistance (R)
 Electrode pitch (C5)
 Electrode voltage drop parameter (VD)
 Applied magnetic field density (BN)
 Nitrogen to Oxygen mole ratio (PSI)
 Fuel mixture ratio (PHI)
 Carbon to hydrogen ratio of the fuel (DELTA)
 Seed concentration in mole % (Q)
 Wall temperature (TW)
 Load current (AMP1)
 Channel distance increment (DX)
 Total channel length (XMAX)
 Time increment (DTAU)
 Initial time (TAU)
 Final time (TAUMAX)
 Number of x-increments (M)
 Number of time steps to be skipped before printing
 the output (IFREQ)
 Number of x-increments for the MHD generator (IDUCT)
 Generator length (DUCT)
 Diffuser length (DIFF)
 Molecular weight of the gas (XM)

The fifth read statement reads the cross-sectional area profile of the channel according to the format (10F.0).

The sixth read statement reads the initial conditions, namely, the stagnation internal energy density (E), the pressure (P), the mass density (RHO) and the momentum density (W) according to the format (5D15.8).

The size of the variables (mass density, velocity, pressure, temperature, momentum density, stagnation internal energy density, Lorentz force, power density, friction force, heat transfer term, current density, electric field, Mach number, area, slope of the area, Hall parameter, electrical conductivity, isentropic efficiency, and internal energy) is determined by the channel length and the channel length increment.

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```
*****
MAIN PROGRAM MHDTO
REQUIRED SUBROUTINES = UPDATE, HGAS
*****
REAL*8 XMAX,TAU,DTAU,TAUMAX,DX,TW,QDIFF,DUCT,DIFF,PMHD,QMHD,PSI,
1 PHI,XI,Q,XKY,BN,AA,VDC,VOC,AMP1,VD,C5,R,PDC,RI,XM,
2 RHO(82),RHON(82),RHON1(82),RHON2(82),W(82),WN1(82),WN2(82),
3 KN(82),E(82),EN(82),EN1(82),EN2(82),V(82),T(82),P(82),
4 P2(82),XJB(82),XJB2(82),XJE(82),XJE2(82),G(82),G2(82),
5 F(82),F2(82),EDC(82),EOC(82),XMACH(82),DADX(82),BETA(82),
6 SIGMA(82),XJX(82),EY(82),EFF(82),ENT(82),XJY(82),ENERGY

INTEGER I,IDUCT,ICT,IFREQ,M
COMMON/XYZ/PSI,PHI,XI,Q
COMMON/ABC/BN,VD,TW,A(82),C4(82),D(82),XKY
READ 10,XKY,AA,R,C5,VD,BN
READ 10,PSI,PHI,DELTA,Q,TW,AMP1
READ 10,DX,XMAX,DTAU,TAU,TAUMAX
READ 10,M,IFREQ,IDUCT,DUCT,DIFF,XM
PRINT 1000,M,IFREQ,XMAX,DTAU,TAUMAX
PRINT 1001,DX,DTAU,DUCT,DIFF
XI=4.0*DELTA/(4.0*DELTA+1)
READ 20,(A(I),I=1,M+1)
*****
READ THE INITIAL CONDITIONS DISTRIBUTIONS FROM A FILE
*****
READ(20,203)((E(I),I=1,M+1),(P(I),I=1,M+1),(RHO(I),IB1,M+1),
1(W(I),I=1,M+1))
DO 40 I=2,M
40 DADX(I)=(A(I+1)-A(I-1))*500/DX
DO 41 I=1,M+1
C(I)=DSQRT(A(I)/AA)
C4(I)=DSQRT(A(I)*AA)
D(I)=2.0*CD0*A(I)/(D(I)+C4(I))
C4(I)=C5/DSQRT(A(I)/AA)
41 CONTINUE
DO 34 I=1,M+1
34 CALL UPDATE(RHO(I),W(I),E(I),P(I),XJB(I),F(I),XJE(I),V(I),G(I),
1T(I),I,IDUCT,EDC(I),EOC(I),AMP1,XM,BETA(I),SIGMA(I),
2XJX(I),EY(I),EFF(I))
DO 3 I=1,M+1
RHON(I)=RHON1(I)=RHON2(I)=WN(I)=WN1(I)=WN2(I)=EN(I)=EN1(I)=0.000
3 EN2(I)=0.000
ICT=0
4 TAU=TAU+DTAU
ICT=ICT+1
```

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171

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*****
IMPLEMENT THE FIRST STEP OF THE TWO-STEP LAX WENDROFF
FINITE DIFFERENCE METHOD
*****
CC 21 I=2,M
  RHON1(I) = ((RHO(I+1)+RHO(I))*DTAU*(W(I+1)-W(I))/DX =
1    DTAU*(W(I+1)*DADX(I)/A(I+1)+W(I)*DADX(I)/A(I)))*.5DO
  RHON2(I) = ((RHO(I)+RHO(I-1))*DTAU*(W(I)-W(I-1))/DX =
1    DTAU*(W(I)*DADX(I)/A(I)+W(I-1)*DADX(I)/A(I-1)))*.5DO
  WN1(I) = ((W(I+1)+W(I))*DTAU*(W(I+1)-W(I))/RHO(I+1) +
2    P(I+1)-W(I)*W(I)/RHO(I)-P(I))/DX = DTAU*(F(I+1)+F(I) -
3    XJB(I+1)-XJB(I))*DTAU*(W(I+1)*W(I+1)*DADX(I)/
4    (RHO(I+1)*A(I+1))+W(I)*W(I)*DADX(I)/(RHO(I)*A(I)))/DX)*
  WN2(I) = ((W(I)+W(I-1))*DTAU*(W(I)-W(I-1))/RHO(I)+P(I)-W(I-1)*W(I-1)/
1    RHO(I-1)-P(I-1))/DX = DTAU*(F(I)+F(I-1)-XJB(I)-XJB(I-1)) -
2    DTAU*(W(I)*W(I)*DADX(I)/(RHO(I)*A(I))+W(I-1)*W(I-1)*
3    DADX(I)/(RHO(I-1)*A(I-1)))/DX)*.5DO
  EN1(I) = ((E(I+1)+E(I))*DTAU*(W(I+1)*(E(I+1)+P(I))/
1    RHO(I+1)-W(I)*(E(I)+P(I))/RHO(I))/DX = DTAU*(G(I+1)+
2    G(I)-XJE(I+1)-XJE(I)+W(I+1)*DADX(I)*(E(I+1)+P(I+1))/
3    (RHO(I+1)*A(I+1))+W(I)*DADX(I)*(E(I)+P(I))/RHO(I)*
4    A(I)))*.5DO
  EN2(I) = ((E(I)+E(I-1))*DTAU*(W(I)*(E(I)+P(I))/RHO(I)-
1    W(I-1)*(E(I-1)+P(I-1))/RHO(I-1))/DX = DTAU*
2    (G(I)+G(I-1)-XJE(I)-XJE(I-1)+W(I)*DADX(I)*(E(I)+P(I))/
3    (RHO(I)*A(I))+W(I-1)*DADX(I)*(E(I-1)+P(I-1))/RHO(I-1)*
4    A(I-1)))*.5DO
21 CONTINUE
DO 22 I=2,M
22 CALL UPDATE(RHON1(I),WN1(I),EN1(I),P(I),XJB(I),F(I),XJE(I),V(I),
1G(I),T(I),I,IDUCT,EDC(I),EOC(I),AMP1,XM,BETA(I),SIGMA(I),
2XJX(I),EY(I),EFF(I))
DO 23 I=2,M
  P2(I)=P(I)
23 CALL UPDATE(RHON2(I),WN2(I),EN2(I),P2(I),XJB2(I),F2(I),XJE2(I),
1V(I),G2(I),T(I),I,IDUCT,EDC(I),EOC(I),AMP1,XM,BETA(I),
2SIGMA(I),XJX(I),EY(I),EFF(I))

```

```

C *****
C IMPLEMENT THE SECOND STEP
C *****
DO 24 I=2,M
  RHON(I)=RHO(I)=DTAU*(WN1(I)=WN2(I))/DX=DTAU*(WN1(I)*DADX(I)/
1    A(I)+WN2(I)*DADX(I)/A(I))*5DC
  WN(I)=W(I)=DTAU*(WN1(I)*WN1(I)/RHON1(I)+P(I)=WN2(I)*WN2(I)/
2    RHON2(I)=P2(I))/DX=DTAU*.5DC*(F(I)+F2(I)=XJB(I)=XJB2(I)=
3    (WN1(I)*WN1(I)*DADX(I)/(RHON1(I)*A(I))+WN2(I)*WN2(I)*
  DADX(I)/(RHON2(I)*A(I)))*DTAU*.5DC
  EN(I)=E(I)=DTAU*(WN1(I)*(EN1(I)+P(I))/RHON1(I)=
1    WN2(I)*(EN2(I)+P2(I))/RHON2(I))/DX=DTAU*.5DC*
2    (G(I)+G2(I)=XJE(I)=XJE2(I)+WN1(I)*(EN1(I)+P(I))*DADX(I)/
3    (RHON1(I)*A(I))+WN2(I)*(EN2(I)+P2(I))*DADX(I)/(RHON2(I)*
4    A(I)))
24 CONTINUE
DO 7 I=2,M
  W(I)=WN(I),E(I)=EN(I)
  7 RHO(I)=RHON(I)
  DO 8 I=1,M+1
  8 CALL UPDATE(RHO(I),W(I),E(I),P(I),XJB(I),F(I),XJE(I),V(I),G(I),
1    I(I),I,INDUCT,EDC(I),EOC(I),AMP1,XM,BETA(I),SIGMA(I),
2    XJX(I),EY(I),EFF(I))
  DO 77 I=1,M+1
  77 EKT(I)=E(I)/RHO(I)*V(I)*V(I)*.5DC
  DO 88 I=1,M+1
  88 XMACH(I)=V(I)/(DSQRT(1.356*P(I)/RHO(I)))
  VCC=VCC+C*.0DC
  DO 9 I=1,INDUCT=1
  VCC=VCC+(EDC(I+1)+EDC(I))*DX*.5DC
  9 VCC=VCC+(EOC(I+1)+EOC(I))*DX*.5DC
  RT=(VCC-VCC)/AMP1
  AMP1=VCC/(RI+R)
  PMHD=QMHD=QDIFF=0.
  DO 5 I=1,M
  IF(I.GT.INDUCT) GO TO 6
  QMHD=QMHD+G(I)*(A(I)+A(I+1))*DX*.5DC
  PMHD=PMHD+XJE(I)*(A(I)+A(I+1))*DX*.5DC
  GO TO 5
  5 QDIFF=QDIFF+G(I)*(A(I)+A(I+1))*DX*.5DC
  CONTINUE
  IF(TAU*GE.C.028DC) GO TO 201
  K=1
  IF(1CT*KE*IFREQ) GO TO 12
  1CT=0
  GO TO 202

```

```

201 CONTINUE
  WRITE(10,203) ((E(I),I=1,M+1),(P(I),I=1,M+1),(RHO(I),I=1,M+1),
1(W(I),I=1,M+1))
  GO TO 1
202 CONTINUE
  DO 25 I=1,IDUCT
    25 XJY(I)=XJB(I)/BN
    PRINT 100,TAU
    PRINT 101
    DO 11 I=1,M+1
      11 PRINT 102,I,P(I),RHO(I),V(I),XJE(I),T(I),XMACH(I),ENT(I)
      WRITE(108,204) (I,BETA(I),SIGMA(I),EDC(I),XJX(I),XJY(I),EY(I),
1EFF(I),I=1,IDUCT)
      PRINT 103,PMHD,QMHD,QDIFF
      PRINT 104,VDC,VOC,AMP1
    12 IF(TAU*GE*TAUMAX) GO TO 1
    GO TO 4
    10 FORMAT(6F.0)
    20 FORMAT(10F.0)
    30 FORMAT(6F.0)
    100 FORMAT(1H1,'AT A TIME TAU =',F15.8,2X,'SEC')
    101 FORMAT(1H0,8X,'PRESSURE',11X,'DENSITY',10X,'VELOCITY',7X,
1'POWER DENSITY',9X,'TEMPERATURE',4X,'MACH',5X,'INT. ENERGY')
    102 FORMAT(2X,I3,2X,E13.7,5X,E13.7,4X,E13.7,5X,E13.7,7X,E13.7,3X,F5.3,
14X,E13.7)
    103 FORMAT(1H0,'MHD POWER OUTPUT =',E14.7,3X,'MHD HEAT LOSS =',E14.7,
13X,'DIFFUSER HEAT LOSS =',E14.7)
    104 FORMAT(1H0,'VDC =',E14.7,3X,'VOC =',E14.7,3X,'AMP1 =',E14.7)
    203 FORMAT(5D15.8)
    204 FORMAT(2X,I3,3X,7E12.5)
    1000 FORMAT('MHD DUCT, ONE DIMENSIONAL TIME DEPENDENT CALCULATIONS')
    1('M' =',I4/('IFREQ' =',I4/('XMAX' =',F6.2/('DTAU' =',
2F10.5/('TAUMAX' =',F10.5))))))
    1001 FORMAT('STEP SIZE'/(2X,'DX' =',F8.5,3X,'METER'/(2X,'DT' =',F8.6,3X,
1'SECOND'/(('MHD DUCT LENGTH' =',F6.2,2X,'METERS'/(('DIFFUSER LENGTH' =
2',F6.2,2X,'METERS')))))
    2000 FORMAT(2X,I3,3X,3E20.6)
    1 OUTPUT AMP1
  END

```

G.2 SUBROUTINE UPDATE

This subroutine calculates the electrical parameters of the MHD generator, the heat transfer and friction terms needed in the program MHDTD.

Input

The input variables required are the mass density, momentum density, stagnation internal energy density, pressure and generator load current.

Nomenclature

CH	-	convective heat transfer coefficient
EG	-	gas emissivity
EW	-	wall emissivity
F	-	friction term
G	-	heat transfer term
H	-	gas enthalpy
QR	-	radiative heat transfer
QW	-	convective heat transfer
RE	-	recovery factor
RED	-	Reynolds number
S	-	Boltzman's constant
XK	-	thermal conductivity
XN	-	viscosity

[illegible]

```

XJE=SIGMA*V*V*BN*BN*(AMP*(C3*BETA+C1*XKY)*(1.DO=VD)=AMP*AMP*
1 (C1*C2+BETA*BETA*C3*C3))/(C2+C1*XKY*XKY)
XJB=SIGMA*V*BN*BN*(AMP*(C1*XKY=C3*BETA)+(1.DO=VD))/(C2+
1 C1*XKY*XKY)
GO TO 4
3 XJE=XJB=0.000
4 EDC=((C1*XKY+C3*BETA)*(1.DO=VD)=(C1*C2+BETA*BETA*C3*C3)*AMP)
1/(C2+C1*XKY*XKY)
EDC=EDC*V*BN
XKC=1.=(C1*C2+BETA*BETA*C3*C3)*AMP/((1.=VD)*(BETA*C3+C1*XKY))
ECC=EDC/XKC
EFF=(AMP*(C1*XKY+BETA*C3)*(1.DO=VD)=AMP*AMP*(C1*C2+BETA*BETA*
1 C3*C3))/(C1*XKY=C3*BETA)*AMP+1.DO=VD)
XJX=SIGMA*V*BN*(AMP*(C2*XKY*BETA*C3)=XKY*(1.DO=VD))/(C2+
1 C1*XKY)
EY=V*BN*XKY*((C1*XKY+C3*BETA)*(1.=VD)=AMP*(C1*C2+C3*C3*BETA*
1 BETA))/(C2+C1*XKY)+V*BN*VD
GO TO 2
1 EDC=ECC=XJB=XJE=0.000
2 CONTINUE
*****
COMPUTE THE HEAT TRANSFER AND FRICTION TERMS
*****
XN=(780.DO+T)*2.1D=08
XK=(270.DO+T)*1.7D=03/XM
RED=RHO*V*D(I)/XN
EGT*(DSQRT(D(I)))*.5D=04
F=.112DO*RHO*V*V/(D(I)*RED**2)
RE=(DABS(CP*XN/XK))*(.33333333)
CH=GAMD*(RED**8)*(XK/(D(I)*CP))
CALL HGAS(TW,P,HWS,S1,S2,S3,I)
QW=CH*(T=HWS+RE*V*V*.5DO)
QR=S*(EG*T*T*T=EW*TW*TW*TW*TW)
Q=4.*(QW+QR)/D(I)
RETURN,END

```

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177

G.3 SUBROUTINE HGAS

This subroutine calculates the thermodynamic and electrical properties of the working fluid.

Input

The input variables needed are gas temperature, gas pressure, seed concentration, fuel mixture ratio, carbon to hydrogen ratio in the fuel and nitrogen to oxygen ratio..

Output

The output variables are gas enthalpy, electrical conductivity, mobility and specific heat (constant pressure) of the gas.

Nomenclature

- CP - specific heat (constant pressure)
- H - gas enthalpy
- SIG - electrical conductivity
- XMU1 - mobility
- XNE - electron density

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```

*****
SUBROUTINE HGAS = CALCULATES THE THERMODYNAMIC AND THE
ELECTRICAL PROPERTIES OF THE WORKING FLUID.
*****
SUBROUTINE HGAS(T2,P,H,SIG,XMU1,CP,K)
REAL*8 XM,H,V,U,Y,X,A,XK2,XK1,TV,G,E,DC,A2,C,BB,T2,P,PSI,PHI,XI,
1 B,XMU1,SIG,XNE,Q,HH(2),CP
COMMON/XYZ/PSI,PHI,XI,Q
IF(K.GT.2) GO TO 11
BB=32.00+28.00*PSI
BB=2.00=XI+PSI
C=5.00=2.00*XI+2.00*PSI
A2=(8.00*XI+4.00)*PHI
DC=4.1870 Q3/(A2+BB)
11 I=1
2 CONTINUE
E=2.00+2.00*(7.00=4.00*XI)*PHI
Q=4.00+(2.00=3.00*XI)*PHI
TV=(3000.00=2000.00*XI+300.00*PSI)/(1.00=5*XI+.09*PSI)
XK1=.390=04*DEXP(.3*XI+34000.00/T2)
XK2=.140=03*DEXP(1.3*XI+29000.00/T2)
A=B/(4.00*XK1*XK1*P*.9870=05)
A=A*.3333333333333333
X=A*(3.00*B+A*XI*C)/(3.00*(1.00+2.00*A*XI)*B+2.00*A**2*XI**2*C)
Y=X/(1.00+.6400*(PHI=1.00)/X+.300*(PHI=1.00)/X)**2)
U=B*(1.00=2.00*XI*X)/(4.00*X*XK1*XK2*P*.9870=05)
V=(2.00*TV/(DEXP(TV/T2)=1.00))*(G+PSI=3.00*Y=U)
H=DC*(T2*(E+7.00*PSI+5.00*Y+3.00*U)+(117.00+30.00*XI)*(Y+PHI=1.00)
1 *1.00 Q3 +135.00*XI*U*1.00 Q3 + V)
IF(I.GT.1) GO TO 1
G=Q*(P*.9870=05)*DSQRT(T2)*DEXP(=50400.00/T2)
G=DSQRT(G)
E=(1.00=XI)*(P*.9870=05)/B
E=E*.6666666666666666
XNE=4.22E 23*G/(1.00+2.00*D=03*(DEXP(16800.00/T2))*E)
XMU1=T2/((P*.9870=05)*((4030.00=3110.00*XI)*(2.72=0457*PSI)
1 +(.15+03*XI)*(0.66*PSI=1.48)*T2+(1050.00=120.00*PSI)*Q+
2 (.05*PSI=13)*T2*Q+(1.1+3.7*PSI)*(XNE*1.00=19)))
SIG=XNE*XMU1*1.60=19
1 HH(I)=H
IF(I.EQ.2) GO TO 3
I=I+1
T2=T2+.1
GO TO 2
3 T2=T2=.1DC
CP=(HH(2)-HH(1))/0.1DC
RETURN;END

```

REFERENCES

REFERENCES

- [1] Rosa, R.J., Magnetohydrodynamic Energy Conversion, McGraw-Hill Book Co., N.Y., 1968, pp. 9-11.
- [2] Faraday, M., "Experimental Researches in Electricity," Philosophical Transactions, Vol. 15, 1832, p. 175.
- [3] Karlovitz, B. and Halasz, D., "History of the K and H Generator and Conclusions Drawn from the Experimental Results," Proceedings of the 3rd Symposium on the Engineering Aspects of MHD, Gordon and Breach, Science Publishers, Inc, 1962, pp. 187-204.
- [4] Lin, S.C., Resler, E.L. and Kantrowitz, A.R., "Electrical Conductivity of Highly Ionized Argon Produced by Shock Waves," Journal of Applied Physics, Vol. 26, 1955, p. 95.
- [5] Rosa, R.J., "An Experimental Magnetohydrodynamic Power Generator," Journal of Applied Physics, Vol. 31, April 1960, pp. 735-736.
- [6] Camac, M. and Janer, G.S., "Applied Magnetohydrodynamics at AVCO-Everett Research Laboratory," Dynamics of Conducting Gases, Northwestern University Press, 1960.
- [7] Patrick, R.M. and Brogan, T.R., "One-Dimensional Flow of Ionized Gas Through a Magnetic Field," Journal of Fluid Mechanics, Vol. 5, Pt. 2, 1959, pp. 289-309.
- [8] Way, S., De Corso, S.M., Hundstad, R.L., Kemeny, G.A., Stewart, W. and Young, W.E., "Experiments with MHD Power Generation," Transaction ASME, Journal of Engineering for Power, Vol. 83A, 1961, pp. 397-406.
- [9] Blackman, V.H., Jones, M.S. Jr. and Demetriades, A., "MHD Power Generation Studies in Rectangular Channels," in C. Mannal and N.W. Mather (Eds), Proceedings of the 2nd Symposium on the Engineering Aspects of MHD, Columbia University Press, New York, 1962, pp. 180-210.

- [10] Sporn, P. and Kantrowitz, A., "Magnetohydrodynamics: Future Power Process?," Power, Vol. 103, No. 11, November 1959, pp. 62-65.
- [11] Steg, L. and Sutton, G.W., "The Prospects of MHD Power Generation," Astronautics, August, 1960, pp. 22-25 ; 84-86.
- [12] Brogan, T.R., "MHD Power Generation," IEEE Spectrum, Vol. 1, No. 2, 1964, p. 58.
- [13] Dinelli, G., Masse, J., "Effect of Various Parameters on the Efficiency of MHD Power Stations," Electricity from MHD, Vol. 3, IAEA, Austria, 1966, p. 799.
- [14] Hoover, D.Q., Somers, E.V., Tsu, T.C., Way, S., Young, W.E., (Westinghouse Electric Corp) and Foster-Pegg, R.W., Koupal, J.R., Spohn, H.C. (Bechtel Corp), Feasibility Study of Coal-Burning MHD Generation, Research and Development Report No. 13, 1966, Office of Coal Research, Department of the Interior.
- [15] Young, W.E., Way, S., Tsu, T.C., and Hoover, D.Q., "Recent Studies of Advanced Coal Burning Power Plants," Inter-Society Energy Conversion Engineering Conference, Proceedings, 1967, pp. 485-492.
- [16] Hals, F.A. and Jackson, W.D., "System Analysis of Central Station MHD Power Plants," Proceedings of the Fifth International Symposium on MHD Electrical Power Generation, Munich, Germany, April 1971, pp. 159-178.
- [17] Bergman, P.D., Plants, K.D., Demeter, J.J. and Bienstock, D., "An Economic Evaluation of MHD-Steam Power Plant Employing Coal Gasification," Report of Investigation No. 7796, Bureau of Mines, 1973.
- [18] Hals, F.A. and Jackson, W.D., "MHD Power Generation - Economic and Environmental Implications," Proceedings of the 10th Symposium on the Engineering Aspects of MHD, Massachusetts Institute of Technology, March 1969, pp. 136-141.

- [19] Lacey, J.J., Demeter, W.H., and Bienstock, D., "Production of a Clean Working Fluid for Coal-Burning Open-Cycle MHD Power Generation," Proceedings of the 12th Symposium on the Engineering Aspects of MHD, Argonne National Laboratory, March 1972, pp. VI.2.1-VI.2.5.
- [20] Feldman, H., Simon, W.H. and Bienstock, D., "Design Data for Coal Burning Open-Cycle MHD Systems: 1. Effect of Slag Formation on Duct Operation and Seed Recovery," Proceedings of the InterSociety Energy Conversion Engineering Conference, Miami Beach, 1967, pp. 423-429.
- [21] Louis, J.F., Lothrop, J. and Brogan, T.R., "Studies of Fluid Mechanics Using a Large Combustion Driven MHD Generator," Physics of Fluids, Vol. 7, March, 1964, pp. 362-374.
- [22] Louis, J.F., Gal, G. and Blackburn, P.R., "Detailed Theoretical and Experimental Study on a Large MHD Generators," Proceedings of the 5th Symposium on the Engineering Aspects of MHD, Massachusetts Institute of Technology, April 1964.
- [23] Dicks, J.B., Wu, Y.L., Powers, W.L., Shanklin, R.V. III, Zitsow, U. and Ziemer, R.E., "An Experimental and Theoretical Comparison of the Performance of Diagonal Wall Generators, Faraday Generators and Hall Current Generators," Proceedings of the 2nd International Symposium on MHD Electrical Power Generation, Salzburg, Austria, Vol. III, 1966.
- [24] Teno, J., Louis, J.F., Westra, L. and Brogan, T.R., "Long Duration Electrodes for Open-Cycle MHD Generators," Proceedings of the 8th Symposium on the Engineering Aspects of MHD, Stanford University, March 1967.
- [25] Mattson, A., Ducharme, E.L., Morrow, I.B. and Brogan, T.R., "Design and Performance of a Single Circuit Output Self-Excited Faraday MHD Generators," Proceedings of the 8th Symposium on the Engineering Aspects of MHD, Stanford University, March, 1967.

- [26] Dicks, J.B., Denzel, J.L., Davis, L.J., Wu, Y.C., Taylor, R., Muehlhauser, J. and Oliver, R.H., "Experimental Study of Diagonal Conducting Wall Generator Using Solid Propellants," AIAA Journal, Vol. 6, No. 9, 1968, pp. 1647-1651.
- [27] Teno, J., Brogan, T.R., Petty, S.W., Lothrop, J.W. and Sonju, O.K., "Research Studies and the Development of MHD Generators and Accelerators," Final Report, Contract AF40 (600) - 1043, October 1969.
- [28] Sonju, O.K., Teno, J. and Brogan, T.R., "Comparison of Experimental and Analytical Results for a 20 MW Combustion Driven Hall Configuration MHD Generators," Proceedings of the 11th Symposium on the Engineering Aspects of MHD, California Institute of Technology, March 1970.
- [29] Rubin, E.S. and Eustis, R.H., "Electrode Size Effects on Combustion Driven MHD Generators," Proceedings of the 11th Symposium on the Engineering Aspects of MHD, California Institute of Technology, March 1970.
- [30] Sonju, O.K. and Teno, J., "An Experimental and Theoretical Investigations of a High Interaction Combustion Driven MHD Generators," Proceedings of the 5th International Conference on MHD Electrical Power Generation, 1971, Munich, Germany, pp. 71-84.
- [31] Pain, H.J., Haines, M.G. and Brown, D.A., "Experimental and Theoretical Studies of MHD Power Generation," Proceedings of the 2nd International Symposium on MHD Electrical Power Generation, Paris, France, 1964, pp. 651-659.
- [32] Yamamoto, M., Tamaoki, T. and Saito, Y., "Experimental Studies of Oil-Fired MHD Power Generators," Electricity from MHD, Vol. 3, 1966, IAEA, Vienna, p. 637.
- [33] Yamamoto, M., Tamaoki, T., Ogiwara, H. and Mawatari, K., "Studies on a Hall Connected MHD Generators," Electricity from MHD, Vol. 5, 1968, pp. 2769-2792.

- [34] Pericart, J. and De Montardy, A., "First Results of the Electricite de France 8 MW (Thermal) MHD Experimental Rig at Centre de Recherches et d'Essais des Renaidiers," Philosophical Transactions, Royal Society, London, Vol. 261A, pp. 868 - 967.
- [35] Kirillin, V.A. and Sheindlin, A.Y., (Editors), MHD Method of Producing Electric Energy, (A Translation of a Russian Language: Magnitogidrodinamicheskiy Metod Polucheniya Elektroenergii, 1972, Energiya, Moscow), JPRS 57940, September 1973.
- [36] USSR High Temperature Institute, A translation of two Russian Language Books: Vazhneyshiye Rezul'taty Nauchno-Issledovatel'skikh Rabot, 1972 Goda; Issledovanya na Ustanovke U-25, JPRS 59983, September 1973.
- [37] Rao, K.R. and Krishnaiah, R., "Three Dimensional Analysis of MHD Generators," IEEE Trans. on Power Apparatus and Systems, Vol. PAS-89, July/Aug 1970, pp. 1237-1241.
- [38] Demetriades, S.T., Argyropoulos, G.S. and Maxwell, C.D., "Progress in the Analytical Modeling of MHD Power Generators," Proceedings of the 12th Symposium on the Engineering Aspects of MHD, Argonne National Laboratory, March 1972, pp. I.5.1-I.5.13.
- [39] Swift-Hook, D.T. and Wright, J.K., "The Constant Mach Number MHD Generator," Journal of Fluid Mechanics, Vol. 16, No. 1, 1962, pp. 97-110.
- [40] Celinski, Z., "Analysis of DC MHD Generator with Stationary Gas Flow," International Symposium on MHD Electrical Power Generation, Proceedings, Paris, France, 1964, pp. 585-599.
- [41] Tsu, T.C.- Young, W.E. and Way, S., "Optimization Studies of MHD Steam Plants," Electricity for MHD, Vol. 3, 1966, pp. 899-910
- [42] Way, S., "Part-Load Operation of MHD Power Plants," Electricity from MHD, Vol. 3, 1966, pp. 913-924.

- [43] Gebel, R., "Optimization of Open-Cycle MHD Power Plant," Electricity from MHD, Vol. 3, 1966, pp. 833-924.
- [44] Rosner, M. and Dzung, L.S., "Efficiencies of Large-Scale Open-Loop MHD Power Generators," Proceedings of the 6th Symposium on the Engineering Aspects of MHD, University of Pittsburgh, April 1965, pp. 39-48.
- [45] Carter, C. and Heywood, J.B., "Optimization Studies on Open-Cycle MHD Generators," AIAA Journal, Vol. 6, No. 9, September 1968, pp. 1703-1711.
- [46] Celinski, Z., "Numerical Calculations of the Electrical Parameters of a Faraday-Type MHD Generator with Two-Dimensional Gas Flow," Report No. 1199, Instytut Badan Jadrowych (Institute of Nuclear Research), 1970.
- [47] Yen, J.T. and Chang, C.C., "MHD Channel Flow Under Time-Dependent Pressure Gradient," Physics of Fluids, Vol. 4, No. 11, November 1961, pp. 1355-1361.
- [48] Lundgren, T.S., Atabek, B.H. and Chang, C.C., "Transient MHD Duct Flow," Physics of Fluids, Volume 4, No. 8, August 1961, pp. 1006-1011.
- [49] Killeen, J., "Computational Problems in MHD," Lawrence Radiation Laboratory, February 8, 1971, UCRL-73038.
- [50] Ryabinin, A.G. and Khozhainov, A.I., "Non-Steady State MHD Flow in Channel," Magnitnaya Gidrodinamika, Vol. 5, No. 3, 1969, pp. 3-7.
- [51] Kazachov, Yu.A., Korotkov, B.A. and Shipulin, I.I., "Non-Stationary Subsonic Flow of a Conducting Gas in Crossed-Electric and Magnetic Fields," Magnitnaya Gidrodinamika, Vol. 3, No. 4, 1967, pp. 29-32.
- [52] Genkin, A.L., Kudryashova, L.A. and Sukhov, G.S., "Non-Steady State Plasma Flow in the Magnetogasdynamic Channel of a Shock Tube," Magnitnaya Gidrodinamika, Vol. 5, No. 3, 1969, -p. 20-26.

- [53] Alivadze, G.R. and Paskonov, V.M., "Calculation of an Unsteady-Flow of a Conducting Gas in the Channel of a MHD Generator," Electricity from MHD, 1968, Vol. 4, pp. 2407-2424.
- [54] Cherepanov, A.N. and Yakovlev, V.I., "Exact Solution of the One-Dimensional Non-Stationary Equations of MHD with Finite Gas Conductivity," Magnitnaya Gidrodinamika, Vol. 6, No. 4, 1970, pp. 11-18.
- [55] Alivadze, G.R., Golovin, V.S. and Khokhryakov, V.V., "Some Problems of the Investigation of Unsteady-State Flow in the Channel of a MHD Generator," MHD Method of Producing Electrical Energy, Part 1, JPRS 57940-1, January 1973, pp. 37-46.
- [56] Rosciszewski, J.J. and Yeh, T.T., "Establishment of Quasi-Steady Condition in a Blowdown Non-Equilibrium MHD Generators," AIAA Journal, Vol. 12, No. 8, August 1974, pp. 1021-1024.
- [57] Adamson, C. and Hingorani, N.G., High Voltage Direct Current Power Transmission, - araway, 1960.
- [58] Cory, B.J., High Voltage Direct Current Converters and Systems, MacDonald, 1965.
- [59] Hingorani, N.G., Hay, J.L. and Crosbie, R.E., "Dynamic Simulation of HVDC System on a Digital Computer," Proceedings IEE (London), Vol. 113, May 1966, pp. 793-802.
- [60] Hingorani, N.G. and Hay, J.L., "Representation of Faults in the Dynamic Simulation of HVDC System by Digital Computers," Proceedings IEE (London), Vol. 114, May 1967, pp. 629-638.
- [61] Hingorani, N.G., Kitchin, R.H. and Hay, J.L., "Dynamic Simulation of HVDC Power Transmission System by Digital Computers," IEEE Trans. Power Apparatus and Systems, Vol. PAS 87, April 1966, pp. 989-996.

- [62] Hingorani, N.G. and Hay, J.L., "Dynamic Simulation of Multi-Converter HVDC System by Digital Computer, Part 1, Mathematical Model," IEEE Trans. Power Apparatus and Systems, Vol. PAS 89, No. 2, February 1970, pp. 218-222.
- [63] Hingorani, N.G. and Hay, J.L., "Dynamic Simulation of Multi-Converter HVDC System by Digital Computer, Part 2, Computer Program," IEEE Trans. Power Apparatus and Systems, Vol. PAS 89, No. 2, February 1970, pp. 222-228.
- [64] Hingorani, N.G. and Mountford, J.D., "Simulation of HVDC System in AC Load Flow Analysis by Digital Computer," Proceedings IEE, Vol. 113, No. 9, September 1966, pp. 1541-1546.
- [65] Chien, K.L., Ergin, E.I., Ling, C. and Lee, A., "Dynamic Analysis of a Boiler," Trans. ASME, Vol. 80, November 1958, pp. 1809-1819.
- [66] Daniels, J.H., Enns, M. and Hottenstine, R.D., "Dynamic Representation of a Large Boiler-Turbine Unit," ASME Paper No. 61-SA-69, ASME Summer Annual Meeting (1961).
- [67] Nicholson, H.J., "Dynamic Optimization of a Boiler," Proceedings of IEE(London), Vol. 111, No. 8, 1964, pp. 1479-1499.
- [68] Thompson, F.T., "A Dynamic Model of a Drum-Type Boiler System," IEEE Trans. Power Apparatus and Systems, Vol PAS 86, May 1967, pp. 625-635.
- [69] Kwan, H.W. and Anderson, J.H., "A Mathematical Model of a 200 MW Boiler," International Journal of Control, Vol. 12, No. 6, 1970, pp. 977-998.
- [70] Speedy, C.B., Bell, R.D. and Goodwin, G.C., "Dynamic Modelling of a Steam Generator Using Least Square Analysis," Joint Automatic Control Conference of The American Automatic Control Council, Proceedings, 1970, pp. 365-372.
- [71] McDonald, J.P., Kwatny, H.G. and Spare, J.H., "A Non-linear Model for Reheat Boiler-Turbine Generator System, Part I- General Description and Evaluation," Joint Automatic Control Conference Proceedings, 1971, pp. 219-226.

- [72] Kwatny, H.G., McDonald, J.P. and Spare, J.H., "A Non-linear Model for Reheat Boiler-Turbine Generator System, Part II - Development," Joint Automatic Control Conference Proceedings, 1971, pp. 227-236.
- [73] McDonald, J.P. and Kwatny, H.G., "Design and Analysis of Linear Regulator Theory," IEEE Trans. on Automatic Control, Vol. AC-18, No. 3, June 1973, pp. 202-209.
- [74] Laxton, J.W. and Steyens, C.G., "Steam Plant Equipment," in G.J. Womack and J.B. Heywood (Eds), Open Cycle MHD Power Generation, Pergamon Press, 1969, pp. 716-751.
- [75] Richtmeyer, R. and Morton, K.W., Difference Methods for Initial Value Problems, 2nd Ed., Interscience Publishers, 1967.
- [76] Pai, S.I., Magnetogasdynamics, Prentice-Hall, Inc., Englewood Cliffs, N.J., 1962.
- [77] Sutton, G.W. and Sherman, A., Engineering Magnetohydrodynamics, McGraw-Hill Book Company, 1965.
- [78] Hughes, W.F. and Young, F.J., The Electromagnetodynamics of Fluids, John Wiley and Sons, Inc., N.Y., 1966.
- [79] Pai, Shih-I, Introduction to the Theory of Compressible Flow, D. Van Nostrand, Princeton, N.J., 1959.
- [80] Shapiro, A., The Dynamics and Thermodynamics of Compressible Fluid Flow, Vol. 1, The Ronald Press Co., N.Y., 1953
- [81] Sutton, G.W., Hurwitz, H. Jr. and Poritsky, H. Jr., "Electrical and End Losses in a Magnetohydrodynamic Channel Due to End Current Loops," Trans. AIEE Communications and Electronics, Vol. 80, Part 1, January 1962, pp. 687-695.
- [82] Dzung, L.S., "Hall Effect and End-Loop Losses in Magnetoplasma-dynamic Generators," Proceedings of the First International Symposium on Magnetoplasma-dynamic Power Generation, Newcastle upon-tyne, England, September 1962, pp. 146-150.

- [83] Sutton, G.W., "End Losses in MHD Channels with Tensor Electrical Conductivity and Segmented Electrodes," Journal of Applied Physics, Vol. 34, No. 2, February 1963, pp. 396-403.
- [84] Neuringer, J.L., "Optimum Power Generation Using a Plasma as The Working Fluid," Journal of Fluid Mechanics, Vol. 7, 1960, pp. 287-301.
- [85] Brocher, E.F., "The Constant Velocity MHD Generator with Variable Electrical Conductivity," Journal of Aerospace Science, Vol. 29, 1962, p. 626.
- [86] Swift-Hook, D.T., Advances in MHD, Ed. by Mcgrath, I.A., Siddall, R.G. and Thring, M.W., Pergamon Press, Oxford, 1963. p. 84.
- [87] Swift-Hook, D.T. and Wright J.K., "The Constant Mach Number MHD Generator," Journal of Fluid Mechanics, Vol. 16, No. 1, 1963, pp. 97-110.
- [88] Bartz, D.R., "Turbulent Boundary Layer Heat Transfer from Rapidly Accelerating Flow of Rocket Combustion Gas and of Heated Air," Advances in Heat Transfer, Vol. 2, Academic Press, N.Y.
- [89] Knudsen, J.G. and Katz, D.L., Fluid Dynamics and Heat Transfer, Research Bulletin No. 37, September 1953, Engineering Research Institute, University of Michigan.
- [90] Teno, J., Liu, C. and Brogan, T.R., "Boundary Layers in MHD Generators," 10th Symposium on the Engineering Aspects of MHD, Proceedings, Massachusetts Institute of Technology, Cambridge, Mass., March 1969, pp. 15-22.
- [91] Shimerin, D.G., "Investigation of the Cooled Channel of the Installation Enin-2," 5th International Conference on MHD Electrical Power Generation, Proceedings, Munich, Germany, 1971, Vol. 1, pp. 249-262.
- [92] Zankl, G., Raeder, J. and Bunde, R., "The Influence of Various Mechanisms on the Flow Parameters of a Seeded Combustion Plasma," 12th Symposium on the Engineering Aspects of MHD, Argonne National Laboratory, March 1972, pp. III.1.1-III.1.6.

- [93] Hale, F. and Kerrebrock, J.L., "Insulators Boundary Layers in MHD Channels," AIAA Journal, Vol. 2, 1964, pp. 461-469.
- [94] Louis, J.F., Lothrop, J. and Brogan, T.R., "Fluid Dynamic Studies in MHD Generators," Physics of Fluids, Vol. 7, 1964, pp. 362-374.
- [95] McAdams, W.H., Heat Transmission, McGraw-Hill Book Co., N.Y., 1954.
- [96] Freck, D.V., "On the Electrical Conductivity of Seeded Air Combustion Products," British Journal of Applied Physics, Vol. 15, 1964, pp. 301-310.
- [97] Way, S., Young, W.E., Tuba, I.S. and Chambers, R.L., "Fuels for Advanced Power Generation Systems," Trans. ASME, Journal of Engineering for Power, Vol. 87A, 1965, pp. 125-147.
- [98] Spencer, F.E. Jr., and Orning, A.A., "An Algorithm for Calculating Multiple-phase Chemical Equilibrium, Bureau of Mines Report on Investigation No. 7786, 1973.
- [99] Zaporowski, B., "Temperature and Physical Properties of Combustion Gases in MHD Generators," International Symposium on MHD Electrical Power Generation, 1968, pp. 2195-2209.
- [100] Baylis, J.A., Carter, C., Coney, M.W.E. and Heywood, J.B., "Duct Physics," in Open Cycle MHD Power Generation, J.B. Heywood and G.J. Womack (Eds), Pergamon Press, 1969 pp. 393-462.
- [101] Frost, L.S., "Conductivity of Seeded Atmospheric Pressure Plasmas," Journal of Applied Physics, Vol. 32, No. 10, October 1961, pp. 2029-2036.
- [102] Pipkin, A.C., "Electrical Conductivity of Partially Ionized Gases," Physics of Fluids, Vol. 4, 1961, pp. 154-158.
- [103] Kruger, C. Jr., Mitchner, M. and Daybelge, U., "Transport Properties of MHD Generator Plasmas," Proceedings IEEE, September 1968, Vol. 56, No. 9, pp. 1458-1471.

- [104] Zemansky, M.W., Heat and Thermodynamics, 3rd Ed., McGraw-Hill Book Co., N.Y., 1951, pp. 412-414.
- [105] Lackner, K., Argyropoulos and Demetriades, S.T., "Relaxation Effects in JXB Devices, AIAA Journal, Vol. 6, 1968, pp. 949-951.
- [106] Harris, L.P. and Cobine, J.D., "Significance of the Hall Effect for Three MHD Generator Configurations," Transactions ASME, Journal of Engineering for Power, October 1961, pp. 392-396.
- [107] De Montardy, A., "An MPD Generator with Series Connected Electrodes," Proceedings of the First International Symposium on Magnetoplasdynamic Electrical Power Generation, Newcastle-Upon-Tyne, England, 1962, pp. 66-68.
- [108] Kessler, R. and Eustis, R.H., "Effects of Electrode and Boundary Layer Temperature on MHD Generator Performance," Proceedings of the IEEE, Vol. 56, No. 9, September, 1968, pp. 1502-1510.
- [109] Vatazhin, A.B., Lyubimov, G.A. and Regires, S.A., "Calculation of MHD Flows in the Channels of MHD Devices," The MHD Method of Producing Electrical Energy, (Eds) V.A. Kirillin and A.Ye. Sheyndlin, Energiya, Moscow, 1968.
- [110] Heywood, J.B., Norris, W.T. and Warren, A.C., "Electrodes and Insulators," in Open Cycle MHD Power Generation, (Eds), G.J. Womack and J.B. Heywood, Pergamon Press, 1969.
- [111] Dzung, L.S., "The Magnetohydrodynamic Generator with Hall Effect at the Duct Ends," Brown Boveri Review, Vol. 49, No. 6, 1962, pp. 211-225.
- [112] Hurwitz, H. Jr., Kilb, R.W. and Sutton, G.W., "Influence of Tensor Conductivity on Current Distribution in a MHD Generator," Journal of Applied Physics, Vol. 32, No. 2, 1961, pp. 205-216.
- [113] Dzung, L.S., "The MHD Generator in Cross-connection," 2nd International Symposium on MHD Electrical Power Generation, Paris, 1964, pp. 601-616.

- [114] Yeh, H. and Sutton, G.W., "Current Output and Efficiency of MHD Generators with Segmented Electrodes and Non-uniform Velocity Profile," First International Symposium on Magnetoplasma-dynamic Electrical Power Generation, Newcastle-upon-tyne, England, 1962, pp. 159-160.
- [115] Schultz-Grunow, F. and Denzel, D.L., "Calculation of the Electric Characteristics of a MHD Generator with Finite Electrodes By Conformal Mapping Under Full Consideration of Tensor Conductivity and a Velocity Profile," 2nd International Symposium on MHD Electrical Power Generation, Paris, 1964, pp. 662-676.
- [116] Dzung, L.S., "Favourable Configuration of Segmented Electrodes for MHD Generators," The Brown Boveri Review, Vol. 53, No. 3, 1966, pp. 238-250.
- [117] Witalis, E.A., "Performance of a Segmented Electrode MHD Generator for Various Electrode - Insulator Length Ratio," Plasma Physics, Journal of Nuclear Energy, Part C, Vol. 7, 1965, pp. 235-244.
- [118] Witalis, E.A., "Analysis of Linear MHD Generators," Plasma Physics, Journal of Nuclear Energy, Part C, Vol. 7, 1965 pp. 455-473.
- [119] Celinski, Z.N., Effect of Finite-Length Electrodes on the Electrical Parameters in a DC MHD Generator with Electrodes Connected in Series," Electricity from MHD, Vol. 2, 1966 IAEA, Vienna.
- [120] Celinski, Z.N. and Fischer, F.W., "Effects of Electrode Size in MHD Generators with Segmented Electrodes," AIAA Journal, V Vol. 4, 1966, pp. 421-438.
- [121] Coney, M.W.E., "The Electrodynamic Effects of Non-uniformity of Magnetic Field and Fluid Parameters in MHD Generators," Plasma Physics, Vol. 10, 1968, pp. 499-514.

- [122] Reseck, K.G., Eustis, R.H. and Kruger, C.H., "MHD Generator Performance Prediction Including Two-Dimensional Effects and Wall Leakage," Proceedings of the 3rd International Symposium on MHD Electrical Power Generation, July, 1966, pp. 687-699.
- [123] Sheindlin, A.E., Shumyatsky, B.Y., Kirillov, V.V., Zalkind, V., and Kovbayuk, V., "Experimental Investigations of the Influence of Boundary Layers and Certain other Effects on The Characteristics of a MHD Generator," Proceedings of the 4th International Symposium on MHD Electrical Power Generation, Warsaw, 1968, pp. 2379-2406.
- [124] Schlichting, H., Boundary Layer Theory, McGrawHill Book Co., 1955.
- [125] Celinski, Z.N., "Electrical Equivalent Circuits of DC MHD Generators," Advanced Energy Conversion, Vol. 7, 1967, Pergamon Press, pp. 57-65.
- [126] Keenan, J.H. and Keyes, F.G., Thermodynamic Properties of Steam, John Wiley, N.Y., 1936.
- [127] STEAM - Its Generation and Use, The Babcock and Wilcox Co., N.Y., 1972.
- [128] Salisbury, J.K., Steam Turbine and Their Cycles, John Wiley, N.Y., 1950.
- [129] Chojnowski, B., Fussey, D.E. and Chew, P.E., "Checker Brick Regenerator," in G.J. Womack and J.B. Heywood (Eds), Open-Cycle MHD Power Generation, Pergamon Press, 1969.
- [130] Kimbark, E.W., Direct Current Transmission, Vol. 1, 1971, John Wiley.
- [131] Marks, J.A., "HVDC: Valid Aid for AC Transmission," Electric Power and Light, T/D Edition, October, 1973, pp. 43-48.
- [132] Gavrilovic, A., Ralls, K.J., Rout, K. and Rowe, B.A., "Application of Thyristor Converter to HVDC Transmission," Power Thyristors and Their Applications, Conference Publication No. 53, IEE, London, 1969, pp. 328-334.

- [133] Gardner, G.E. and Wright, D.G., "Application of Thyristors to HYDC Transmission," Power Thyristors and Their Applications, IEE, 1969, pp. 347-352.
- [134] Hay, J.L. and Naik, K.R., "Application of Thyristors to High Voltage Direct Current Power Transmission - Sequential Firing," Power Thyristors and Their Applications, IEE, 1969, pp. 383-385.
- [135] Ohno, E., Mitsouka, H. and Knimura, Y., "Thyristor Strings for High Voltage Applications," Power Thyristors and Their Applications, IEE, 1969, pp. 406-412.
- [136] Jones, K.M., Whalley, H. and Cure, J.R., "The Prospects of Using Thyristor Strings in Distribution Development," Power Thyristors and Their Applications, IEE, 1969, pp. 422-428.
- [137] Bedford, D.D. and Hoft, R.G., Principles of Inverter Circuits, John Wiley and Sons, 1964.
- [138] Gutzwiller, F.W., (Ed), SCR Manual, General Electric Co., N.Y., 1967.
- [139] Mulica, A.R., "How to Use Silicon Controlled Rectifiers in Series and Parallel," Control Engineering, Vol. 11, May, 1964, pp. 95-99.
- [140] Dortort, I.K., "Current Balancing Reactor for Semiconductor Rectifiers, Trans. AIEE, Vol. 77, 1958, pp. 452-456.
- [141] Burstein, S.Z., "Finite-difference Calculation for Hydrodynamic Flows Containing Discontinuities," Journal of Computational Physics, Vol. 2, 1967, pp. 198-222.
- [142] Courant, F.K. and Lewey, H., "On the Partial Differential Equations of Mathematical Physics," IBM Journal, March 1967, pp. 215-234.
- [143] Roache, P.J., Computational Fluid Dynamics, Hermosa Press, 1972, pp. 228-230.

- [144] Owczarek, J.A., Fundamentals of Gas Dynamics, International Textbook Co., Scranton, Pa., 1964.
- [145] Thomas, L.H., "Computation of One-Dimensional Flow Including Shock," Communications in Pure and Applied Mathematics, Vol. 7, pp. 195-206.
- [146] Lax, P., "Weak Solutions of Non-linear Hyperbolic Equations and Their Numerical Computations," Communications in Pure and Applied Mathematics, Vol. 7, pp. 159-193.
- [147] Lax, P. and Wendroff, B., "System of Conservation Laws," Communications in Pure and Applied Mathematics, Vol. 13, 1960, p. 217.
- [148] Henrici, P., Discrete Variable Methods in Ordinary Differential Equations, John Wiley, N.Y., 1962.
- [149] Collatz, L., The Numerical Treatment of Differential Equations, Translated from a Supplemented Version of the German Edition by Williams, P.G., Springer-Verlag, Berlin, 1960.
- [150] Ralston, A. and Wilf, H., Mathematical Methods for Digital Computers, John Wiley, 1960, pp. 159-216.
- [151] Ralston, A., A First Course in Numerical Analyses, McGraw-Hill Book Co., N.Y., 1965, pp. 93-128.
- [152] Keenan, J.H., Keyes, F.G., Hill, P.G. and More, J.G., Steam Tables - Thermodynamics Properties of Water Including Vapor, Liquid and Solid Mass, John Wiley and Sons, N.Y., 1969.
- [153] Hilsenrath, J., et al, Tables of Thermodynamics and Transport Properties of Gases, Pergamon Press, N.Y., 1960.
- [154] Oliver, D.A. and Byer, P.H., Physical and Economic Models of Open Cycle Coal Burning MHD Power Generation System, Gas Turbine Laboratory, MIT, June 1972.
- [155] Oliver, D.A., "The Time Dependent Magnetohydrodynamic Generator," 14th Symposium on the Engineering Aspects of MHD, Proceedings, University of Tennessee, Tullahoma, April, 1974, pp. V.5.1-V.5.9.

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